On the performance evaluation of lithium-ion battery systems for dynamic load functions in warship hybrid power and propulsion systems

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Statement of Originality

I, Luke Farrier confirm that the work presented in this thesis is my own. Where information has been derived from other sources, I confirm that this has been indicated in this thesis.

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Abstract

Battery technology has developed to a juncture where high power and high energy density characteristics can be exploited for a common use battery energy storage system (ESS) for warship power systems to improve system steady state and dynamic performance. A critical review of previous research has exposed a lack of knowledge in performance assessment of battery ESS to operate as power reserve, to load level generator sets and supply laser directed energy weapons (LDEW) in a warship hybrid power and propulsion system.

This research explores the performance impact of using a battery ESS in a candidate hybrid power and propulsion system. A simulation model of a lithium-ion nickel manganese cobalt based ESS was developed and validated against high rates of charge and discharge. Three system models were developed to explore the steady state, quasi-steady state and dynamic performance of the candidate power system when the battery is integrated.

Three key investigations were conducted using the respective system models. The first explored the effects of ESS on the candidate power system performance when the ESS is operated as power reserve. Analysis showed that a 40% reduction in exhaust greenhouse gas (GHG) emissions was potentially achievable from the candidate warship compared to conventional operating practice. The second explored power system performance when operating the ESS operates to load level a diesel generator under quasi-steady state conditions. A 2% droop limit is suggested to mitigate against adverse quality of power supply (QPS) conditions for electrical consumers.

The third investigation, and key contribution to the field of naval power systems, explored the impact of LDEW demands on the transient response of the ESS and system quality of power supply. The research findings show that the battery ESS is capable of high rates of fire for extended periods subject to state of charge operating limitations. To mitigate against adverse QPS conditions and provide operators with a realistic operating envelope to power the laser with the battery ESS, it is recommended that the power limit of the laser load should be 1.75 MW peak power.
Impact Statement

Pulsed weapon integration with, and decarbonisation of warship power and propulsion systems are two of the key challenges that are currently faced by the naval industry. Pulsed weapons are being integrated as they are claimed to offer fast engagement times, conduct precision engagements and conduct graduated response to threats. Reducing ship greenhouse gas (GHG) emissions is a key objective of the International Maritime Organisation, who legislate commercial shipping. While naval ships are exempt from this category, governments are acting on the critical need to reduce emissions. This puts pressure on their navies to reduce the emissions footprint of their warships.

This research investigates the application of a battery ESS with Li-ion NMC cells to provide power to laser directed energy weapons (LDEWs), and the ability of the same ESS to reduce the fuel consumption and exhaust GHG emissions of a candidate warship hybrid power and propulsion system. Development of a battery ESS simulation model representative of an ESS, proven in the commercial ship environment, was undertaken based upon experimental test data provided by Corvus Energy. The originality of the investigation, is the use of this state-of-the-art technology for loading conditions commensurate with naval warship operation. The significance is that the investigation de-risks a real world ESS, for future experimental testing when powering LDEWs, and when operating to load level diesel generator sets on naval warships. Li-ion NMC based battery systems have yet to be explored in the literature for LDEW power supply, placing further significance on the results of this research.

The key stakeholders that the research findings impact are battery ESS designers, the naval power system design authority, warship operators and the environment. The impact to battery ESS designers is the identification of performance limitations specific to the NMC technology when powering LDEW loads. These limitations aim to prevent damage to the cells in the system caused by transient impact of the load.

The following impacts pertain to both the naval power system design authority and warship operators. The transient characteristic of the LDEW load in this research is managed by a novel converter control circuit that interfaces the battery ESS and the LDEW, this design ensures compliance with quality of power supply (QPS) standards during LDEW operation. The design ensures proper operation of the LDEW, and adequate QPS to electrical consumers. The second impact is the identification of constraints for state of charge of the battery ESS under LDEW operation. These constraints are suggested so that the LDEW operating duration requirement for future warships is met.

Power sharing design constraints are recommended to the power system design authority to mitigate against adverse QPS under quasi-steady state conditions during load levelling operation. A significant potential reduction in GHG emissions from the candidate warship of up to 40% was identified when the ESS is operated in power reserve, compared to conventional operating practice of the candidate warship.
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<td></td>
</tr>
<tr>
<td>$C_b$</td>
<td>Base capacitance</td>
<td></td>
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<td>$C_f$</td>
<td>Filter capacitance</td>
<td></td>
</tr>
<tr>
<td>$C_1$</td>
<td>First RC pair capacitance</td>
<td></td>
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<tr>
<td>$C_2$</td>
<td>Second RC pair capacitance</td>
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<td>$D$</td>
<td>Switch duty cycle ratio</td>
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<td>V</td>
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<td>$H_v$</td>
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<td>$i(h)$</td>
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<td>$K_e$</td>
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<td>Governor gain</td>
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<td>$K_i$</td>
<td>Integral gain</td>
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<td>DG power set point from the power management system</td>
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<td>$P_{ESS^*}$</td>
<td>ESS power set point from the power management system</td>
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<td>(#)</td>
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<td>( P_{G,\text{Nonline}} )</td>
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<td>( Q_{\text{cell}} )</td>
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<td>( R_b )</td>
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<td>( r_d )</td>
<td>Diode on state resistance</td>
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<td>( R_d )</td>
<td>Damping resistance</td>
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<td>( R_m )</td>
<td>Magnetization resistance</td>
<td>(\Omega)</td>
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<td>( R_0 )</td>
<td>Cell internal resistance</td>
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<td>( R_{1t} )</td>
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<td>$t_{22}$</td>
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<td>Rotor angular velocity</td>
<td>Rad/s</td>
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<td>$\omega_{\text{res}}$</td>
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### Abbreviations

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<td>Absolute Percentage Error</td>
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<tr>
<td>AFE</td>
<td>Active Front End</td>
</tr>
<tr>
<td>AC</td>
<td>Alternating Current</td>
</tr>
<tr>
<td>Ah</td>
<td>Amp hour</td>
</tr>
<tr>
<td>ASW</td>
<td>Anti-Submarine Warfare</td>
</tr>
<tr>
<td>AVR</td>
<td>Automatic Voltage Regulator</td>
</tr>
<tr>
<td>BMS</td>
<td>Battery Management System</td>
</tr>
<tr>
<td>BoL</td>
<td>Beginning of Life</td>
</tr>
<tr>
<td>CAPEX</td>
<td>Capital Expenditure</td>
</tr>
<tr>
<td>CO₂</td>
<td>Carbon Dioxide</td>
</tr>
<tr>
<td>CO</td>
<td>Carbon Monoxide</td>
</tr>
<tr>
<td>CODLOG</td>
<td>Combined Diesel Electric or Gas Turbine</td>
</tr>
<tr>
<td>CCM</td>
<td>Continuous Conduction Mode</td>
</tr>
<tr>
<td>DoD</td>
<td>Depth of Discharge</td>
</tr>
<tr>
<td>DG</td>
<td>Diesel Generator</td>
</tr>
<tr>
<td>DC</td>
<td>Direct Current</td>
</tr>
<tr>
<td>DCM</td>
<td>Discountinuous Conduction Mode</td>
</tr>
<tr>
<td>DP</td>
<td>Dynamic Positioning</td>
</tr>
<tr>
<td>EV</td>
<td>Electric Vehicle</td>
</tr>
<tr>
<td>EM</td>
<td>Electromagnetic</td>
</tr>
<tr>
<td>EMCAT</td>
<td>Electromagnetic Catapult system</td>
</tr>
<tr>
<td>EMC</td>
<td>Electromagnetic Compatibility</td>
</tr>
<tr>
<td>EMKIT</td>
<td>Electromagnetic Kinetic Integrated Technology</td>
</tr>
<tr>
<td>EF</td>
<td>Emission Factor</td>
</tr>
<tr>
<td>EoL</td>
<td>End of Life</td>
</tr>
<tr>
<td>ESS</td>
<td>Energy Storage Systems</td>
</tr>
<tr>
<td>EFCM</td>
<td>Equivalent Fuel Consumption Minimisation</td>
</tr>
<tr>
<td>ESR</td>
<td>Equivalent Series Resistance</td>
</tr>
<tr>
<td>FCF</td>
<td>Fuel Conversion Factor</td>
</tr>
<tr>
<td>FEC</td>
<td>Full Equivalent Cycle</td>
</tr>
<tr>
<td>GT</td>
<td>Gas Turbine</td>
</tr>
<tr>
<td>GTA</td>
<td>Gas Turbine Alternator</td>
</tr>
<tr>
<td>GP</td>
<td>General Purpose</td>
</tr>
<tr>
<td>GHG</td>
<td>Greenhouse Gas</td>
</tr>
<tr>
<td>HIL</td>
<td>Hardware-In-The-Loop</td>
</tr>
<tr>
<td>HVAC</td>
<td>Heating Ventilation and Cooling</td>
</tr>
<tr>
<td>Abbreviation</td>
<td>Definition</td>
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<tr>
<td>--------------</td>
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</tr>
<tr>
<td>HTS</td>
<td>High Temperature Superconducting</td>
</tr>
<tr>
<td>LCL</td>
<td>Inductor-Capacitor-Inductor</td>
</tr>
<tr>
<td>IEEE</td>
<td>Institute of Electrical and Electronics Engineers</td>
</tr>
<tr>
<td>IGBT</td>
<td>Insulated Gate Bipolar Transistor</td>
</tr>
<tr>
<td>IFEP</td>
<td>Integrated Full Electric Propulsion</td>
</tr>
<tr>
<td>IMO</td>
<td>International Maritime Organization</td>
</tr>
<tr>
<td>LDEW</td>
<td>Laser Directed Energy Weapon</td>
</tr>
<tr>
<td>LaWS</td>
<td>Laser Weapon System</td>
</tr>
<tr>
<td>Li-Air</td>
<td>Lithium Air</td>
</tr>
<tr>
<td>LCO</td>
<td>Lithium Cobalt Oxide</td>
</tr>
<tr>
<td>Li-ion</td>
<td>Lithium Ion</td>
</tr>
<tr>
<td>LFP</td>
<td>Lithium Iron Phosphate</td>
</tr>
<tr>
<td>LMO</td>
<td>Lithium Manganese Oxide</td>
</tr>
<tr>
<td>Li-S</td>
<td>Lithium Sulphur</td>
</tr>
<tr>
<td>LTO</td>
<td>Lithium Titanate Oxide</td>
</tr>
<tr>
<td>LV</td>
<td>Low Voltage</td>
</tr>
<tr>
<td>MGO</td>
<td>Marine Gas Oil</td>
</tr>
<tr>
<td>MCR</td>
<td>Maximum Continuous Rating</td>
</tr>
<tr>
<td>MBTO</td>
<td>Mean Time Between Overhaul</td>
</tr>
<tr>
<td>MV</td>
<td>Medium Voltage</td>
</tr>
<tr>
<td>CH₄</td>
<td>Methane</td>
</tr>
<tr>
<td>NCA</td>
<td>Nickel Cobalt Aluminium</td>
</tr>
<tr>
<td>NMC</td>
<td>Nickel Manganese Cobalt</td>
</tr>
<tr>
<td>NOₓ</td>
<td>Nitrogen Oxides</td>
</tr>
<tr>
<td>N₂O</td>
<td>Nitrous Oxide</td>
</tr>
<tr>
<td>NMVOC</td>
<td>Non-Methane Volatile Organic Compounds</td>
</tr>
<tr>
<td>NRMSE</td>
<td>Normalised Root Mean Squared Error</td>
</tr>
<tr>
<td>NATO</td>
<td>North Atlantic Treaty Organization</td>
</tr>
<tr>
<td>OCV</td>
<td>Open Circuit Voltage</td>
</tr>
<tr>
<td>OPEX</td>
<td>Operational Expenditure</td>
</tr>
<tr>
<td>PFN</td>
<td>Pulse Forming Network</td>
</tr>
<tr>
<td>PM</td>
<td>Particulate Matter</td>
</tr>
<tr>
<td>PLL</td>
<td>Phase Locked Loop</td>
</tr>
<tr>
<td>PMS</td>
<td>Platform Management System</td>
</tr>
<tr>
<td>PHIL</td>
<td>Power Hardware-In-The-Loop</td>
</tr>
<tr>
<td>PTI/PTO</td>
<td>Power Take In/Power Take Off</td>
</tr>
<tr>
<td>PI</td>
<td>Proportional Integral</td>
</tr>
<tr>
<td>PWM</td>
<td>Pulse Width Modulation</td>
</tr>
<tr>
<td>QPS</td>
<td>Quality of Power Supply</td>
</tr>
<tr>
<td>RAS</td>
<td>Replenishment at Sea</td>
</tr>
<tr>
<td>RC</td>
<td>Resistor-Capacitor</td>
</tr>
<tr>
<td>RHP</td>
<td>Right-Hand Plane</td>
</tr>
<tr>
<td>RMSPE</td>
<td>Root Mean Square Percentage Error</td>
</tr>
<tr>
<td>RN</td>
<td>Royal Navy</td>
</tr>
<tr>
<td>RBC</td>
<td>Rule Based Control</td>
</tr>
<tr>
<td>SGO</td>
<td>Single Generator Operation</td>
</tr>
<tr>
<td>SEI</td>
<td>Solid Electrolyte Interphase</td>
</tr>
<tr>
<td>Abbreviation</td>
<td>Description</td>
</tr>
<tr>
<td>--------------</td>
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</tr>
<tr>
<td>SSL</td>
<td>Solid State Laser</td>
</tr>
<tr>
<td>SFC</td>
<td>Specific Fuel Consumption</td>
</tr>
<tr>
<td>STANAG</td>
<td>Standardisation Agreement</td>
</tr>
<tr>
<td>SoC</td>
<td>State of Charge</td>
</tr>
<tr>
<td>SOx</td>
<td>Sulphur Oxides</td>
</tr>
<tr>
<td>SSE</td>
<td>Sum of Squares due to Error</td>
</tr>
<tr>
<td>THD</td>
<td>Total Harmonic Distortion (up to 3000 Hz)</td>
</tr>
<tr>
<td>UPS</td>
<td>Uninterruptable Power Supply</td>
</tr>
<tr>
<td>UK</td>
<td>United Kingdom</td>
</tr>
<tr>
<td>US</td>
<td>United States</td>
</tr>
<tr>
<td>UAV</td>
<td>Unmanned Aerial Vehicles</td>
</tr>
<tr>
<td>VSC</td>
<td>Voltage Source Converter</td>
</tr>
</tbody>
</table>
Chapter 1 Introduction

1.1 Motivation

Battery technology is poised for mass market adoption in the automotive sector in order to reduce greenhouse gas (GHG) emissions (Liu et al., 2019). This has accelerated the development of lithium-ion (Li-ion) battery technology in the last decade towards batteries with high energy and high power density, high coulombic efficiencies and low self-discharge rate characteristics to achieve automotive sector requirements (Kim et al., 2019). The contest to develop advanced Li-ion technology has provided an opportunity of transferrable benefits, applicable to parallel industries that could take advantage of these battery characteristics. Commercial marine electric power systems have exploited these characteristics of battery energy storage systems (ESS) to improve fuel economy and reduce exhaust GHG emissions (Alnes et al., 2017; Stefanatos et al., 2015), and provide fast response to high-ramp rate thruster demands for vessels that require dynamic positioning (Mutarraf et al., 2018; Southall and Ganti, 2018) such as offshore support vessels. These benefits could also complement operation of warship power systems, where an opportunity exists to investigate the use of battery technology to power pulsed weapon systems. This research is in support of the UK MoD’s efforts to investigate the de-risking of ESS to supply future pulsed weapon systems.

Future warships are predicted to embark advanced weapon systems, the power for which will be drawn from the ships electric power system. Among these advanced systems are high power laser directed energy weapons (LDEWs) characterised by a pulsed power profile (Doerry et al., 2015; Gattozzi et al., 2015; Mills et al., 2018; O’Rourke, 2019). Such pulsed weapons offer fast engagement times, the ability to counter rapidly manoeuvring missiles, conduct precision engagements and conduct graduated response to threats (O’Rourke, 2019). The supply of pulsed power is an important factor driving change in power system design, and is considered a primary requirement for the integration of energy storage devices that have fast response dynamics capable of driving pulsed loads for sustained periods (Lowe et al., 2018; McCoy, 2015). Warships with an Integrated Full Electric Propulsion (IFEP) system may have large generators with sufficient ramp rate capability to power megawatt level laser loads. However, this is not necessarily the case for ships with inherently reduced power generation capacity, such as ships with a hybrid electric and mechanical propulsion system (Mills et al., 2018). This is because it is yet unknown whether smaller
Generator sets, characteristic of hybrid propulsion systems, can provide the intake of combustion air and fuel flow rates to meet the ramp rate demand of high power LDEW loads. Hence there is need to investigate appropriate ESS that can facilitate LDEW pulsed power loads for use on ships with hybrid power and propulsion systems.

Recently the UK Royal Navy (Tate and Rumney, 2017) and U.S. Navy (O’Rourke, 2019) have announced investment in the development of LDEWs, with future power requirements envisioned to reach megawatt level. The UK Royal Navy have announced the intent to trial LDEW systems on naval ships by 2023 and expectation for LDEW systems to be on the frontline by 2029 (Ministry of Defence, 2019). This accelerates the need to understand how this particular weapon could be powered by an ESS. The integration of an ESS is intended to alleviate the concerns expressed in the naval engineering community on the impact of the transient characteristic of the pulse load on the ships power generation components (Gattozzi et al., 2015; Mills et al., 2018; Tate and Rumney, 2017). The LDEW load is required to operate for up to a four minute engagement period as discussed by Markle (2018), which makes drawing the power from the ships generators impractical. Hence this places a large power and energy requirement on an ESS.

Previous research has begun to develop an understanding of powering pulsed laser loads in ship power systems using capacitive (Southall and Ganti, 2018) and flywheel based ESS (Jennett et al., 2019; Tate and Rumney, 2017), this is because of their high power density and cycle life capability characteristics. However, the technological advancements in battery technology have reached a stage where land-based testing of Li-ion based battery systems for use in warship power systems has begun (Langston et al., 2019). This has accelerated the need to understand how batteries respond to LDEW loading. Therefore, a research need exists to investigate the integration of battery ESS with a candidate warship power system. Second, to explore the ability the battery ESS to meet the demand of LDEW loading, in order to establish operating constraints required to meet the demand of the LDEW without causing damage to the battery ESS.

The added advantage of battery ESS is that when a warship is not required to operate to supply power to an LDEW during warfighting, a battery ESS could improve power system performance during low threat operations and therefore offers a potentially higher level of versatility to the warship than capacitive or flywheel based ESS. These secondary operating modes in low threat operations include power reserve and load levelling (Southall and Ganti, 2018; Zohrabi et al., 2019). While previously published research has investigated the power reserve, load levelling and LDEW power supply operating modes in isolation, research on the ability of a battery ESS to operate in each mode for a candidate warship hybrid power system is limited. Therefore, there is a research need to develop understanding of specifying a battery ESS to meet the requirements of the three operating modes, where the battery ESS should be integrated in the power system architecture, and how the power system performs when the battery ESS is integrated.

This thesis aims to provide guidance to battery ESS designers and the naval power system design authority on integrating battery based ESS with warship hybrid power and propulsion systems. The objectives of the guidance is to ensure that the resulting battery ESS is correctly specified to match operating mode requirements and that the resulting warship hybrid power system design will satisfy the requirements of operating LDEW loads. Moreover, to ensure that the battery ESS functions within its acceptable operating envelope to prevent damage to the battery cells in the ESS during dynamic operating modes.
1.2 Research aims

The questions this research aims to answer when integrating battery ESS with warship hybrid power and propulsion systems are:

1) What are the implications, consequences and constraints on warship power system performance when a battery ESS operates as power reserve?

2) What are the implications, consequences and constraints on power system performance during load levelling operating conditions, whilst ensuring acceptable levels of QPS?

3) What are the implications, consequences and constraints on power system and battery ESS operation, to ensure acceptable levels of QPS and prevent damage to the battery ESS under LDEW operation?

This leads to the following research aims on the integration of ESS for dynamic functions:

1) Carry out a literature based investigation into the key ESS operating modes in ship power systems, examine how energy stores are controlled in ship power systems and review the state-of-the-art of battery ESS technology for warship power system integration. Provide a critical review of existing literature on battery ESS integration with ship power systems to identify the key research challenges.

2) To provide conceptual understanding and justification of how a battery ESS is integrated with a power and propulsion system of a candidate warship. Consequently, theoretically describe how the ESS operates to meet the requirements of power reserve, load levelling and LDEW power supply operating modes.

3) Construct analytical and time-domain modelling tools of the power system under investigation that are capable of exploring power system performance when a battery ESS operates in the power reserve, load levelling and LDEW power supply operating modes. Justify the selection of constituent components of the modelling tools and discuss their limitations. Conduct model verification and validation to attest the credibility of the models to yield credible results that support the development in understanding of power system performance with integrated battery ESS.

4) Analytically investigate warship power system performance over operating profiles that are commensurate with warship operation, when the battery ESS is operating in power reserve. Quantify fuel consumption, exhaust GHG emissions and engine running hours. Compare the performance with conventional operating practice for warship power systems that do not have integrated battery ESS. Discuss the implications and consequences resulting from using a battery ESS in the power reserve mode in warship power systems.

5) Investigate the impact of operating a battery ESS in load levelling on power system QPS, fuel consumption and exhaust GHG emissions using simulation based methods. Explore power sharing settings to ensure adequate QPS. Discuss the implications and consequences resulting from using
a battery ESS in load levelling mode in warship power systems and compare findings with the power reserve mode.

6) Investigate the transient impact of LDEW loading on battery ESS at beginning of life and when degraded. Investigate the system transient response, and assess the impact on system QPS resulting from LDEW operations. Discuss the implications and consequences of the LDEW load on the performance of the battery ESS and power system. Suggest constraints that aim to mitigate damage to the battery ESS and any adverse QPS conditions that may arise from LDEW operations. Provide recommendations with rationale on the use of battery ESS to supply power to LDEW loads.

### 1.3 Thesis outline

This thesis is divided into the following 7 chapters:

**Chapter 1 Introduction.** This chapter provides an introduction to the research undertaken and the motivation behind it. The research question, aims and contributions are described. The publications on the research topic of ESS are presented.

**Chapter 2 Literature review.** This chapter provides a thorough review of the published literature in the field of battery ESS and their integration with warship electric power systems. This review includes forming an understanding of battery operating modes in ship power systems and the state-of-the-art in battery technology. The use of battery ESS for dynamic load operating modes is critically reviewed to identify the key challenges in the field.

**Chapter 3 Problem formulation.** This chapter describes a conceptual understanding of how a battery ESS is integrated with a candidate electric power system to act in three operating modes; power reserve, load levelling and LDEW power supply. This chapter also specifies the battery ESS and interface design with the candidate electric power system, to address the problem of electrically integrating and controlling a battery ESS to meet the requirements of the three operating modes.

**Chapter 4 Modelling.** Based upon analysis of the research problem in chapter 3, this chapter describes three modelling tools developed to explore the research problems identified for each operating mode. A steady state tool is developed for operating profile analysis under power reserve operation. The development of a quasi-steady state tool is described to investigate load levelling performance. The third is a dynamic tool constructed to explore the dynamic performance of the candidate battery and system QPS under LDEW operation. This chapter also presents verification and validation of the modelling tools.

**Chapter 5 Power reserve and load levelling modes.** This chapter describes two investigations conducted using modelling tools 1 and 2, described in chapter 4, into the battery ESS power reserve and load levelling operating modes. This chapter presents the results of the two investigations and then discusses the implications and consequences of the results generated for battery ESS operation, and warship electric power systems.
Chapter 6 LDEW power supply modes. This chapter describes the investigations undertaken to explore the ability of the battery ESS to meet the demands of LDEW pulsed loads at beginning of life, and under degraded conditions. Results are presented and key observations are made. The implications and consequences of using the battery ESS to power LDEW loads on the battery and power system performance are discussed, and subsequent recommendations are made.

Chapter 7 Conclusions and future work recommendations. A summary of the research findings is presented. This chapter details the recommendations on the use of the battery ESS in the three operating modes investigated for warship electric power systems that have been contributed by this research. Future work recommendations are suggested.

1.4 Research contributions

The following novel contributions are claimed by the author:

1) Development of a validated Nickel Manganese Cobalt (NMC) based battery simulation model. This research has developed and validated a high-fidelity behavioural battery time-domain model that is based upon Li-ion cells with NMC chemistry. The novel aspect of this contribution is that the simulation model response has been validated against battery module level experimental results, for modules developed by Corvus Energy, that are employed in commercial ship power system applications. This is presented in chapter 4 and published in Farrier et al. (2019).

2) Proposition of a battery based pulsed power converter control system. This research has contributed a DC/DC converter control system that has been proven to be capable of maintaining quality of power supply (QPS) under LDEW loading. This is presented in chapter 4 and published in Farrier et al. (2019).

3) Development of a model with which to explore the steady-state performance of a warship electric power system with battery based energy storage. This research has contributed a model capable of quantifying warship power system performance when a battery ESS is integrated, cognisant of power supply efficiency characteristics. The model is detailed in chapter 4.

4) Development of a model with which to explore the operating limits of the battery ESS under LDEW operation. This research has contributed a modelling tool capable of simulating the transient impact of LDEW operation on a battery ESS. The tool is detailed in chapter 4.

5) Recommendations on the droop slope settings under parallel operation of a diesel generator and battery ESS during load levelling. This research has contributed constraints on the droop slope of the voltage source converter interfacing the ESS with the warship power systems. These constraints ensure adequate quality of power to consumers by limiting the deviations in system voltage under quasi-steady state conditions during load levelling. This is detailed in chapter 5.

6) Recommendations on the battery ESS operating limitations when powering LDEW. This research has contributed operating constraints under which the battery must be maintained, during LDEW operation to ensure that the requirement for duration of LDEW operation for future surface warships is met and that the transient impact of the load does not cause damage to the battery ESS.
This research has also contributed cooling system implications of operating the LDEW with the battery ESS at beginning of life and under degraded performance, to ensure that the battery ESS is maintained at safe temperatures during LDEW operation. This is presented in chapter 6, published in Farrier and Bucknall (2019) and presented in Farrier and Bucknall (2020).

7) **Recommendations on power system operation when powering LDEWs with the battery ESS.** This research has contributed recommendations on compliance with quality of power supply standards under LDEW loading with the battery ESS as the power source. This ensures proper operation of the LDEW and electrical consumers that are sensitive to power quality. This is detailed in chapter 6 and Farrier and Bucknall (2020).

### 1.5 Contributions to the literature

As a consequence of the research presented in this thesis the following academic papers have been published or submitted for publication.

**Journal paper:**


**Conference papers:**


**Concurrent conference papers published related to energy storage systems**

Chapter 2  Literature review

2.1  Introduction

This review addresses four main themes. These themes are; battery ESS operating modes in ship electric power systems, how battery ESS are managed at the system level in hybrid ship power systems, the state-of-the-art in battery technology, and the integration and management of battery ESS within warship power systems. This chapter presents the literature review in four parts:

1) The aim of the first part is to provide an overview of the ESS operating modes for commercial and naval ship electric power systems, and to understand how and why ESS are being used in the shipping environment.

2) The aim of the second part is to review how power is managed between sources in ship and terrestrial microgrid power systems to understand how a battery energy store might be controlled when operating in conjunction with conventional shipboard power generation technology.

3) The aim of the third part is to review the state-of-the-art of battery ESS suitable for warship power system integration, to equip the reader with a description of the fundamental battery characteristics, and their physical limitations, and examine trends in battery technologies.

4) The aim of the fourth part is to critically review the recent work on battery ESS integration with ship power systems to identify the key challenges posed and the contributions that have been made in this research field to identify where knowledge is lacking.

Prior to the four key parts a background of warship power system state of the art is presented to provide research field context.

2.1.1  Background to warship power system state of the art

There are two fundamental architectures of the electric warship; segregated and integrated full electric. The electric warship concept as described by Hodge & Mattick (1995), details the provision of electric propulsion by employing electric propulsion motors, supplied by prime mover generators via electrical distribution, as opposed to using the prime movers for mechanical power transmission, either directly or
through a gearbox. Electric propulsion can be categorised into hybrid or IFEP, as discussed by Little et al. (2003). In the former, mechanical and electric drive systems are combined, but the electric propulsion capability is limited to a lower ship speed and the mechanical propulsion portion is utilised to obtain the higher vessel speeds with minimum transmission losses (McCoy, 2015). The latter uses a common power source for the ship services and electric propulsion.

**Power generation**

In ship electric propulsion, power generation is most commonly delivered by mechanical prime movers (either diesel engines or gas turbines) driving synchronous alternators providing fixed frequency AC using wound fields to control the generated voltage (Calfo et al., 2002; McCoy, 2002). Improving power density and fuel efficiency is a prominent naval research area in power generation for the electric ship due to increased on-board power demand. Dual wound generators have been argued by Hodge and Eastham (2015) and Rashkin et al. (2018a) to facilitate power density improvement. The dual wound generator concept enables the supply to multiple subsystems from a single generator set. This could either be as high voltage supply for propulsion and low voltage for ship service supply (Hodge and Eastham, 2016), or the separation of supply to the port and starboard buses to improve efficiency from running the generator at higher load factor (Rashkin et al., 2018a). Alternative technologies include water-cooled wound field, permanent magnet and high temperature superconducting (HTS) generators (Calfo et al., 2007; O’Regan et al., 2015).

**Power distribution**

Present distribution architectures in warship power systems are either categorised into split, radial, ring or zonal (Dalton and McCoy, 2014). The most common type being radial, as employed on the IFEP RN Type 45 (Partridge and Thorp, 2014), HMS Queen Elizabeth (Eaton and Webster, 2018), and hybrid Type 26 design (McNaughtan et al., 2016), the latter shown in Fig. 2-1(a). In radial systems, power is distributed from a minimum of two separate sources, each with dedicated switchboards that are connected by a bus tie, to the ship’s load. Zonal architectures have been identified for future power systems, these are likely to be employed to increase survivability, as each electrical zone is split transversely to align with watertight bulkheads, with each zone comprising a power conversion unit to supply power at the lower voltage level (Doerry et al., 2015).

Traditional warship architectures are primarily based upon 3-wire AC distribution (Meggs and Pollard, 2016), although DC architectures for naval vessels have experienced a resurgence in publications, arguably initiated by the Electric Warship series (Hodge & Mattick 1995, 1996, 1997; Newell et al. 1999; Hodge & Mattick 1999, 2000). More recently, the US Office of Naval Research have been investing in research to develop Medium Voltage DC (MVDC) zonal distribution (Fig. 2-2) for future destroyer and littoral combat ship designs (Doerry et al., 2015; Doerry and Amy, 2018; Zohrabi et al., 2019). At present, the first in-service system as a step toward MVDC, is the 1kV LVDC system installed on the DDG 1000 from the US Navy’s Zumwalt Class, as shown in Fig. 2-1(b). The zonal system is supplied by a radial network of four Gas Turbine Generators with a total rating of 82 MW, operating at 4.16 kV AC (Doerry and Amy, 2017).
There are potential benefits in migrating to a MVDC zonal distribution from AC architectures (Castellan et al., 2018; R. D. Geertsma et al., 2017; Zohrabi et al., 2019). With reference to the system presented in Fig. 2-2, these claimed benefits include:

- The system is regulated by power electronic converters, thus allowing faster power system dynamics and elimination of reactive power flow in the main distribution system.

- Phase angle synchronisation is not required between sources and loads, thus simplifying the connection and disconnection procedures of power generation and energy storage devices.

- The frequency constraints on prime movers is removed due to the power electronic interface, therefore prime movers can operate asynchronously and at variable speed, allowing potential reduction in specific fuel consumption. This can also reduce generator size and weight, as generator frequency is proportional to volume for the same power rating (Vijlee et al., 2007).

- Reduces or possibly eliminates the requirement for bulky low frequency transformers and mitigation for the associated inrush currents.

- Improved management of fault currents, power flow and reconfiguration in transient and emergency conditions.
However, there remains a number of significant challenges (R. D. Geertsma et al., 2017; Zohrabi et al., 2019):

- Cost and losses inherent to the power electronic devices. Including those for the associated cooling system design.
- Nitrogen Oxide (NOx) increase inherent to operation of diesel engine prime movers at variable speed.
- Dielectric and capacitive behaviour, due to increased common mode currents producing magnetic fields in power cables, being a source of electromagnetic interference, that has been associated with bearing and insulation failures (Brovont and Pekarek, 2015; Doerry and Amy, 2018).
- Control system complexity and heterogeneity, reducing system reliability (Vu et al., 2017c).
- Network stability due to high ramp rate loads (Vu et al., 2017a).
- Interrupting DC fault current. Unlike AC distribution, DC is unidirectional and does not naturally cross zero every half cycle. Therefore, a high speed fault interruption characteristic is required. This could be provided by solid-state converters and/or hybrid solutions, (mechanical and solid state to avoid on-state and switching losses).

The cost-benefit analysis for future warship platforms may favour MVDC distribution, however systems are still in the experimental stage (Sudhoff et al., 2015). At present the predominant distribution topology is AC.

**Loads**

Warship power consumers can be categorised as ship service, propulsion and combat system loads. For hybrid and IFEP systems, the ship service load, comprising hotel loads and auxiliary systems such as, fuel pumps, fresh water and chilled water systems may account for between 10 and 20% of the total installed power.

The state-of-the-art for electric propulsion motors in naval application is considered to be the Advanced Induction Motor. The Advanced Induction Motor is in-service on the Type 45 destroyer (Saunders and Humphrey, 2013), HMS Queen Elizabeth (Eaton and Webster, 2018) and DDG 1000 (LaGrone, 2016). The objective of the Advanced Induction Motor is to provide a low speed and high torque machine, characterised by high power density, low structure borne noise and a large air gap for robustness to withstand shock for naval applications (Lewis, 2002). Considering future propulsion machines, these may compromise the form of permanent magnet machines, to exploit their high power density. HTS motors may be utilised in the future, these machines have a high temperature superconducting field winding and require cryogenic cooling. The beneficial characteristics of HTS, as described by Kirtley et al. (2015) and Thongam et al. (2013) include high air gap flux density, high power and torque density, high efficiency and enhanced electrical stability.
Warship combat system loads comprise weapons and sensors. High powered electronic weapons, sensors and pulsed power loads are being developed for integration with future surface combatants (Doerry et al., 2015; Zohrabi et al., 2019). Future combat system technologies impacting integrated electric power systems and wider platform design include laser directed energy weapons (LDEWs), electromagnetic (EM) railguns and high power radar (Doerry et al., 2015). McCoy (2015) and Lowe et al. (2018) argued that these loads are the primary drivers of change in the approach to power and propulsion system design. The benefits of directed energy weapons and EM railguns are high power (lower time to target) and precision at low cost per shot (Petersen et al., 2011), whilst removing the need for conventional magazines, or reduction in magazine size (Tate and Rumney, 2017). The lower cost per shot is contributed to the accelerating force of the beam or projectile being derived from the ship’s power system and not a chemical propellant (Meggs and Drywood, 2015).

**EM railguns**

From an operational perspective, the purpose of EM railguns is to provide naval surface fire support, land attack, ship defence and enemy deterrent (Office of Naval Research, 2017). The EM railgun requires high current to pass through the rails and armature in order to propel a projectile using the Lorentz force. Consequently an EM railgun will draw a large pulse of energy from the ship’s power system, reported as up to 160 MJ over a 6-10 ms pulse (Whitelegg et al., 2015), equating to approximately 16 GW of pulse power. Where the rate of fire for EM railguns is required to be between 10 and 12 shots per minute (Huhman et al., 2016; Whitelegg, 2016a).

Owing to the high power, short pulse demand characteristics of EM railguns, drawing the pulse energy directly from the generator sets is impractical because of the prime mover operating limits and power system impedance (Whitelegg et al., 2015). The intermittent pulses from EM railguns and LDEWs give rise to the requirement for an alternative electrical power delivery source, encompassed in an ESS system to support these pulse power loads, the highest predicted demand being placed from EM railguns (Hebner et al., 2015). The GW-level short duration pulses required by EM railguns are generated by a Pulse Forming Network (PFN). As described by Huhman et al., (2016) and Whitelegg (2016), the PFN operates by charging an ESS device, most commonly a capacitor, over a period of seconds to a set point voltage based on the amount of desired stored energy in the system. When the railgun load is ready, thyristors are activated via an inductive pick-up loop to synchronize all of the switching devices. Current flows through the thyristors, into a pulse-forming output inductor to shape the current pulse required by the EM railgun.

**Laser Directed Energy Weapons**

There are three types of ship deployable LDEW under development, fibre solid state lasers (SSL), slab SSL and free electron lasers (Petersen et al., 2011). Many navies including the Royal Navy (Tate and Rumney, 2017) and U.S. Navy (O’Rourke, 2018) have invested in developing LDEWs, with the power requirement envisioned to be at the megawatt level, thus representing a significant proportion of power generation. In 2014 the U.S. Navy installed and tested a 33 kW prototype solid state Laser Weapon System (LaWS) on the USS Ponce similar to that in Fig. 2-3 (O’Rourke, 2018). Free electron lasers are still in the laboratory development phase, with a reported optical power of 14.7 kW (O’Rourke, 2015).
Fig. 2-3. LaWS SSL prototype on the USS Ponce (U.S. Naval Institute, 2014)

Future LDEWs are predicted to have power requirements of 2 MW after losses and have efficiencies between 20 and 30% owing to waste thermal energy (Gattozzi et al., 2015; O’Rourke, 2018; Petersen et al., 2011). Gattozzi et al. (2015) assumed SSL optical powers are likely to be between 30 kW and 125 kW. If a thermal efficiency of 25% is considered, this would equate to a maximum SSL power of 500 kW, lower than the predictions of Petersen et al. (2011). More recently the SSL load demands have been identified as up to 2 MW with a 2.5 s on time and 40% duty cycle (Mills et al., 2018), and is required to be operated for a period of four minutes (Markle, 2018). The state-of-the-art and predicted technology development of laser weapons is summarised in Table 2-1.

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Power after thermal losses</td>
<td>132 kW</td>
</tr>
<tr>
<td></td>
<td>120-500 kW</td>
</tr>
<tr>
<td></td>
<td>0.25-2 MW</td>
</tr>
<tr>
<td>On time</td>
<td>Not stated</td>
</tr>
<tr>
<td></td>
<td>6 s</td>
</tr>
<tr>
<td></td>
<td>2.5 s</td>
</tr>
<tr>
<td>Duty cycle</td>
<td>67%</td>
</tr>
<tr>
<td></td>
<td>33% and 50%</td>
</tr>
<tr>
<td></td>
<td>40%</td>
</tr>
<tr>
<td>Reference</td>
<td>(O’Rourke, 2015)</td>
</tr>
<tr>
<td></td>
<td>(Gattozzi et al., 2015)</td>
</tr>
<tr>
<td></td>
<td>(Mills et al., 2018)</td>
</tr>
</tbody>
</table>

Studies have approximated the SSL load profile as a cyclically repeating pure square wave (Allan and Jones, 2015), however SSL load profiles are trapezoidal in shape with a short rise and fall time with high di/dt (Kim et al., 2014; Scuiller, 2012). This is caused by a delay characteristic known as time to full radiant intensity of the laser aperture, this is in the tens of milliseconds (Titterton, 2015). The characteristics of the SSL load profile that challenge warship power systems are magnitude of the peak pulse power, load rise and fall time, and the repetition rate of the load. Meggs and Drywood (2015), Gattozzi et al. (2015), Tate and Rumney (2017) and Mills et al. (2018) agreed on the need for integrated electrical ESS to generate the pulse with high di/dt for the SSL demand and to alleviate the ship power system and prime movers from the effects of the high power load transient. Mills et al. (2018) highlighted that this is important for warships with hybrid power and propulsion systems with smaller prime mover generator sets. Such prime movers are physically limited by their ability to intake combustion air, which in practice cannot occur instantaneously. Moreover, the transient, cyclic nature of the load subjects engines to repeated thermal stress which could reduce engine component life and reduce mean time between overhaul.

Meggs and Drywood (2015) further argued that the key integration challenge for current platforms without ESS is the LDEW energy requirement. Without an ESS, this gives rise to the requirement to either utilise
Literature review

prime mover spinning reserve or reduction in ship speed to provide the required power capacity. The other challenges identified are cooling and system control. Gattozzi et al. (2015) and Tate and Rumney (2017) have further argued that the transient power requirements and cooling requirements (due to high inefficiencies) of SSLs present integration challenges with the power system with regard to energy management.

Comparison of EM railgun and LDEW load characteristic

The pulse load pattern characteristics for LDEWs and EM railguns are compared using their respective power vs time profiles in Fig. 2-4(a) and (b). The load characteristic plot in Fig. 2-4(a) for the LDEW was generated using the combination of the state-of-the-art for an SSL, the power output and duty cycle from Mills et al. (2018), and the time to full radiant intensity from Titterton (2015). The PFN discharge characteristic in Fig. 2-4(b) follows the profile exhibited in Huhman et al. (2016). The key differences between each profile to note are the peak pulse power, load rise time and pulse duration, these are compared numerically in Table 2-2. The EM railgun clearly imparts a significantly more dynamic load with regard to rise time and peak power relative to the LDEW, hence the EM railgun necessitates a PFN and in practice a very high power energy storage device (Lowe et al., 2018; Whitelegg, 2016a).

![Fig. 2-4. Characteristic plot of (a) LDEW (SSL type) and (b) EM railgun load profiles](image)

<table>
<thead>
<tr>
<th>Parameter</th>
<th>LDEW</th>
<th>EM railgun</th>
</tr>
</thead>
<tbody>
<tr>
<td>Peak pulse power</td>
<td>2 MW</td>
<td>16 GW</td>
</tr>
<tr>
<td>Duration of pulse</td>
<td>2.5 s</td>
<td>6-10 ms</td>
</tr>
<tr>
<td>Load rise time</td>
<td>10’s ms</td>
<td>~ ms</td>
</tr>
<tr>
<td>Duty cycle</td>
<td>40%</td>
<td>0.1%</td>
</tr>
<tr>
<td>Required stored energy per shot</td>
<td>1.4 kWh</td>
<td>44 kWh</td>
</tr>
</tbody>
</table>

Due to the high power characteristic of an EM railgun, they are primarily being developed for IFEP platforms (Doerry and Amy, 2018). This concurs with Daffey and Hodge (2004), who offer analysis on the
feasibility of high energy weapons for IFEP and hybrid propulsion plants, concluding that integrating EM pulsed loads with hybrid propulsion systems have low feasibility due to the inherent reduced installed power generation. However, lower power LDEWs could be considered for both IFEP or hybrid propulsion systems due to the lower power requirements (Mills et al., 2018).

While EM railgun loads present a number of integration challenges with IFEP platforms including power management, thermal management and Quality of Power Supply (QPS) (McNab, 2014; Whitelegg et al., 2015). The research focus here is placed on hybrid power and propulsion systems, for the purpose of this research this is defined as a warship with a means of providing propulsion via mechanical or electric drive. The decision to focus the research here is first, due to the recent announcement that the UK are expecting to trial LDEWs on Royal Navy ships by 2023 and be on the frontline by 2029 (Ministry of Defence, 2019). Second, that the UK and US are testing energy storage for laser weapons systems that may feature on platforms with hybrid power and propulsion in the near future (Defence Science and Technology Laboratory, 2019). This decision was made to increase the relevance and direct usefulness to the naval industry.

2.2 Energy storage system operating modes in shipping

This section of the literature review first discusses the historical motivation within the field to integrate ESS. The following subsections are subdivided by the ESS operating modes that are pertinent to warships and commercial ships, due to transference of the performance benefit and challenges associated with ESS integration.

This chapter classifies hybrid power supply as the combination of conventional power generation sources (DGs and GTs) and the stored power of an ESS. This is not to be confused with the definition for hybrid power and propulsion, which is associated with hybrid electric and mechanical drive used to propel the ship. An example ship with hybrid power supply and electrical propulsion is shown in Fig. 2-5 with DGs and electrochemical battery ESS. As shown in Fig. 2-5 there are three key locations that could be considered when integrating the energy store:

1) Integrated with the main distribution bus via a DC/AC converter
2) Via the DC link of the propulsion converter (with or without DC/DC converter)
3) With the low voltage distribution system.
Literature review

2.2.1 Background of ESS in warships

The emergence of ESS in warships is an evolution of battery-supported DC systems in submarine programmes. Hodge & Mattick (1995) noted that energy storage devices such as batteries on surface warships could negate the need for multiple storage devices common to warships, using the example of hydraulically powered steering gear and stabilisers that rely on fluid based energy stores in accumulators to allow control of surfaces on the loss of hydraulic pumps in the supply. Hodge and Mattick (1995) proposed the use of a lead acid battery, leveraged from submarine applications on a DC ship service ring main (Fig. 2-6), citing its use for action load and propulsion up to 12 knots (kts) for 30 minutes or for back up supply. The opportunities identified as a consequence of employing a central electrical ESS in naval ships was further expanded upon by Hodge and Mattick (1996, 1997, 1999). Noting single generator operation with a battery system as back up, instead of dual generator operation, to increase fuel efficiency therefore reducing operational expenditure (OPEX), using a battery energy storage device as the sole power source for silent operation to reduce vulnerability by reducing acoustic noise and heat from combustion engine exhaust gas, generator/propulsion ride through during failure or damage, and dedicated supply of pulsed power.

Despite these potential benefits, Saunders and Humphrey (2013) noted that ESS use is still conservative on naval vessels. This agrees with Smith (2010) who acknowledged that there has been a lack of ESS other than at the local UPS level on the Type 45. Tate and Rumney (2017) further argued that the number of different small scale UPS, potentially of different technologies and cell types, imposed additional maintenance, complexity and cost. This is supported by Kuseian (2015), who argues the need for minimising the number of UPS at equipment level to reduce cost and maintenance.
More recently the benefits of ESS that have been identified include increased survivability, energy recovery during a crash stop deceleration, black start capability following a black-ship condition and load levelling/inertia compensation to improve fuel efficiency and reduce exhaust GHG emissions (Bellamy and Bray, 2015). Hebner et al. (2015) concurred with the opportunities identified by Hodge and Mattick (1999, 1997, 1996, 1995) and Bellamy and Bray (2015), and further argued that the increasing load variability requires additional electrical ESS integrated with the power system. Moreover, Hebner et al. (2015) goes on to state that large scale electrical ESS is a method to provide faster response to differential load changes as ESS are not limited by the characteristics of the prime mover, thereby facilitating reduction of engine maintenance and increasing service life, commensurate with the more recent conclusions of Southall and Ganti (2018) and Zohrabi et al. (2019).

2.2.2 ESS operating mode 1: Power reserve

It is documented practice for warships to run a minimum of two generators at sea (Allen and Buckingham, 2017; Hebner et al., 2010; Mahoney et al., 2012; Southall and Ganti, 2018). This assures continuity of power supply by having sufficient spinning reserve should a fault or unscheduled shut down occur to one of the prime movers. The benefit of continuity of power supply comes at the cost of higher specific fuel consumption (SFC) due to operating the prime movers at low, non-optimised load levels, increased operating hours, reduced availability and higher total cost of ownership (Hebner et al., 2010). An ESS can potentially reduce the total cost of ownership and enable reduced or single generator operation by substituting spinning reserve with short-term power reserve to provide power to consumers until an alternative prime mover is brought online. Power reserve utilises the power and energy from the ESS to increase the generator loading margins, subject to available state of charge in the ESS. The time period for power reserve is between one to ten minutes, this depends upon whether the prime mover is a DG or GT respectively (Herbst and Gattozzi, 2012; Southall and Ganti, 2018) and if the prime mover is on auto-start standby or requires starting from cold (Kim et al., 2014).
2.2.3 ESS operating mode 2: Load levelling and peak shaving

The largest energy loss for a single stage in the power system is during the fuel conversion process within the prime mover, which is exacerbated when operating at low load. An ESS connected to the power system via a power electronic converter with fast bandwidth controllers can facilitate the fast absorption or delivery of power under dynamic load conditions. ESS load levelling has the potential to improve operating efficiency, by reducing fuel consumption and exhaust GHG emissions by decoupling the load from the prime mover and handling power fluctuations, allowing near constant loading of the prime mover close to its optimum efficiency point (Bordin and Mo, 2019; R. D. Geertsma et al., 2017). This is illustrated by Fig. 2-7. Temporary increases in load demand handled by the ESS is described as peak shaving, an example of which may be during manoeuvring where fast response is required, this mitigates against the potential requirement for intermittent generator start/stops (Bordin and Mo, 2019; Radan et al., 2016), thus increasing mean time between overhaul (MTBO). With no ESS an additional generator must be online to supply the load when the power peaks during vessel operation, thus potentially reducing operating efficiency. The duration of peak shaving support depends on the ESS state of charge and whether the ESS is simultaneously operating as a power reserve.

![Graph showing load levelling and peak shaving](image_url)

**Fig. 2-7. ESS (a) load levelling and (b) peak shaving modes during generic load profiles**

Dynamic load conditions where an ESS could benefit system performance, include variable thruster loads during manoeuvring (Stefanatos et al., 2015), fluctuating propulsion loads in high sea states (R. D. Geertsma et al., 2017) drilling/crane operations (Ovrum and Bergh, 2015) and energy capture from EM Launch (Southall and Ganti, 2018). Levelling highly dynamic pulsed loads with an ESS can alleviate stresses on the prime mover, a topic covered in detail in section 2.2.4.

Viking Lady, an offshore supply vessel, was the first large commercial vessel to integrate a large Li-ion battery ESS with the power system in the early 2010s via an active rectifier as shown in Fig. 2-8 (Stefanatos et al., 2015). The focus of their paper was on the fuel savings, rather than the electrical performance of the system. The authors claimed that over the operating profile, which included dynamic positioning activity,
projected fuel savings were in the order of 15%. Since Viking Lady, there has been a notable increase in the number of Li-ion battery ESS installations in offshore supply vessels as demonstrated in Fig. 2-9.

![Diagram of Viking Lady power and propulsion system (Stefanatos et al., 2015)](image)

**Fig. 2-8.** Viking Lady power and propulsion system (Stefanatos et al., 2015)

![Bar chart showing number of offshore vessels with installed Li-ion battery ESS (Eknes, 2018)](image)

**Fig. 2-9.** Number of offshore vessels with installed Li-ion battery ESS (Eknes, 2018)

### 2.2.4 ESS operating mode 3: Laser pulsed power supply

To sustain the high energy weapon load profiles discussed in section 2.1.1, increased flexibility in the power system is required. This is due to the transient impact that the pulsed loads have on prime mover loading that could be detrimental to engine life and quality of power supply, in terms of frequency and voltage deviations (Lowe et al., 2018; Mills et al., 2018).

Using simulation techniques Mills et al., (2018) investigated the frequency response of a validated 3 MW high speed DG model when subjected to the LDEW pulsed load parameters stated in Table 2-1 against NATO STANAG 1008 QPS criteria. The authors’ results indicated that the electrical frequency response could be maintained within the maximum 5.5% limit set out in STANAG 1008. However, they go on to discuss that whilst this may be acceptable from a QPS perspective, there are physical limitations that need
to be considered under cyclic loading that are a cause for concern, citing ramp rates to achieve the necessary intake of combustion air and thermal stresses. The authors concluded that the cyclic nature of the load may lead to excessive thermal cycling, reduction of component life and reduced MTBO, thus increasing through life cost. This could be exacerbated given that the laser is required to operate for up to a four minute engagement period as discussed by Markle (2018). The work of Mills et al., (2018) did not include the use of an ESS to contribute power to the LDEW pulse load.

Lowe et al. (2018) concur with Mills et al. (2018), and go on to state that a method of providing the flexibility required to manage the large stresses imparted by these demanding load dynamics on QPS and power generation components is by integrating an ESS with the power system architecture. This is in agreement with the conclusions of Herbst et al. (2015), Whitelegg et al. (2015) and T. Van Vu et al. (2017) due to the inherently fast acting response of an ESS (Hebner et al., 2015).

**EM launch**

While the EM railgun and EM catapults themselves are not the primary focus of this research, it is important to understand the state-of-the-art of how an ESS facilitates the mitigation of the load transient from a power system perspective.

The presence of an EM railgun load on a ships electrical power system requires an ESS to rapidly transition from supplying and absorbing energy to meet the firing repetition rate of 12 rounds per minute (Herbst et al., 2015). As the discharge power and charge duration of the intermediate store between the ESS and the weapon are critical, power density of the ESS device is prioritised over energy density, which complements the characteristics of capacitors over battery technology (Wetz et al., 2014). This concurs with McNab (2014), who agrees that batteries are not yet capable to drive EM railgun loads directly. An intermediate ESS between the generator sets and the EM railgun is likely required to maintain power system stability and mitigate the voltage and frequency transients in the ship power system caused by the high power, short duration load characteristic. Through time-domain simulation techniques, Whitelegg et al. (2015) demonstrated that by decoupling a 64 MJ muzzle energy EM railgun load transient from a 36 MW gas turbine alternator with a 305 MJ capacitive ESS as depicted in Fig. 2-10, the frequency and voltage transients could be managed to within NATO STANAG 1008 QPS limits. The author justified the 305 MJ capacitor ESS compared to the 160 MJ per shot requirement, in order to retain a residual charge sufficient to reduce the inrush current, and therefore maintain QPS within limits, upon recharge of the capacitor bank.

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**Fig. 2-10. 36 MW gas turbine alternator supplying EM railgun load via capacitive ESS (Whitelegg et al., 2015)**
An alternative approach to the decoupling of EM railgun load transients from ship power systems by Huhman et al., (2013) and Huhman and Wetz (2015) was to develop a hybrid ESS approach in a laboratory demonstrator, where a battery charges a capacitive pulsed power circuit at 12 kJ/s to 5 kV in 5 s before a shot is taken. The cycle is then repeated at 10 cycles per minute for 5 minutes. This load profile is similar to that of an LDEW, and will be considered in more depth in section 3.3 of chapter 3.

More recent works from T. V. Vu et al. (2017a) and Gonsoulin et al. (2018) have used model predictive control techniques to manage the emulated power electronic interfaces of distributed power sources in a 1:10000 scale version of the 12 kV MVDC power system previously presented in Fig. 2-2. This approach demonstrated the feasibility of using advanced power management techniques to manage the power imbalance between generators, ESS and an EM railgun load by optimising the ramp rate coordination between them whilst maintaining power system voltage stability. The authors did not explicitly state the ESS technology simulated in their work. Similarly, Mardani et al. (2019) used model predictive control to investigate the mitigation of high power pulsed loads using a hybrid ESS, where a Li-ion battery charges a supercapacitor bank to power the load at 2 Hz, 20% duty cycle.

It should be noted that as yet, the only ESS, with the exception of small UPS scale, installed in a warship power system is a rotating machine fitted to the U.S. Navy’s nuclear powered aircraft carrier, the USS Gerald R. Ford. The ESS is the source of pulsed power to the EM catapult launch system which is integrated with the ships electric power system to provide aircraft launch capability (Doyle et al., 1995; Hebner et al., 2015). In the UK the MV EM CATapult (EMCAT) (Lewis and Butcher, 2004) and LV EM Kinetic Integrated Technology (EMKIT) (Southall et al., 2017) shore based demonstrators were developed to support the launch of large airframes and unmanned aerial vehicles (UAVs) respectively, the energy requirements for which have been detailed by Lewis et al. (2009) and are summarised in Table 2-3 for a launch rate of one airframe per minute.

<table>
<thead>
<tr>
<th>Discharge time (s)</th>
<th>Discharge Energy (MJ)</th>
<th>Stored Energy (MJ)</th>
<th>Aircraft</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.5</td>
<td>2</td>
<td>4</td>
<td>Small UAVs</td>
</tr>
<tr>
<td>2.5</td>
<td>6</td>
<td>12</td>
<td></td>
</tr>
<tr>
<td>2.5</td>
<td>17.5</td>
<td>35</td>
<td>Full size combat aircraft</td>
</tr>
<tr>
<td>2.5</td>
<td>35</td>
<td>70</td>
<td></td>
</tr>
</tbody>
</table>

The topology of the EMCAT launch system is described in Fig. 2-11. The system comprises two supercapacitor ESS that are charged by generators via separate transformers and dedicated bi-directional PWM converters at a controlled rate. When the supercapacitors reach rated voltage and state of charge, the charge and launch contactors switch over, decoupling the launch phase from the generator, facilitating the supercapacitor to discharge to the linear induction motors that launch the airframe in accordance with the discharge times stated above in Table 2-3. The pulsed power requirements make for good comparison with the pulsed weapon load characteristics.
2.2.5 ESS operating mode 4: Sole power source

If the ESS is of sufficient capacity and power rating it can act as the sole power source to the ship power system loads. In commercial shipping there are two distinct categories, the first being vessels with primarily battery based ESS, such that the role of the ship can be carried out on battery power alone and recharged by shore power, similar to the Norled Ampere (Corvus Energy, 2019). The second category being vessels with conventional electric propulsion supplemented by a large ESS installation to provide power to ship services and propulsion for a limited period such as the MS Roald Amundsen and RRS Sir David Attenborough (Stevens et al., 2017). For a commercial ship with electric propulsion there are two key operational benefits of using battery ESS for sole power. The first being zero exhaust GHG emissions during battery only operation, thus protecting the environment from harmful pollutants. The second is reduction in the acoustic signature, which for example can improve passenger comfort, aid research vessel operation and protect aquatic mammals through reduced acoustic pollution from rotating machinery. These benefits are not only transferrable but are also desirable for a warship with electric propulsion from a capability perspective, by reducing the underwater radiated noise signature from rotating machinery and the heat from exhaust gas (Newell et al., 1999).

2.2.6 ESS operating mode 5: Regeneration

During fast reversal of a propeller shaft, the convention is to utilise dynamic braking resistors to absorb regenerative energy by this process (Lewis, 2006). This is also the case for davit lifting and lowering operations (Radan et al., 2016). Alternatively the regenerative energy could be stored in an ESS and then re-cycled to assist other propulsion manoeuvres (Bellamy and Bray, 2015).

2.2.7 Ship ESS operating mode summary

Table 2-4 summarises the primary ESS operating modes, system performance benefits and technologies considered for integration within a naval ships electrical power distribution system.
Table 2-4: Summary of ESS operating modes and performance benefits for shipping

<table>
<thead>
<tr>
<th>ESS operating mode</th>
<th>Claimed power system performance benefit</th>
<th>ESS state of the art</th>
<th>References</th>
</tr>
</thead>
<tbody>
<tr>
<td>1. Power reserve</td>
<td>Back-up power source under failure or casualty of a generator set. Reduced generator or single generator operation, therefore reduced fuel consumption and exhaust GHG emissions. Reduced running hours, therefore increased MTBO. Reduce vulnerability to faults or damage.</td>
<td>Li-ion batteries.</td>
<td>Herbst and Gattozzi, (2012) Kim et al., (2014) Southall and Ganti, (2018)</td>
</tr>
<tr>
<td>4. Sole power source</td>
<td>Eliminates exhaust GHG emissions. Reduced acoustic signature from rotating machinery during operations.</td>
<td>Li-ion batteries.</td>
<td>Stevens et al. (2017)</td>
</tr>
<tr>
<td>5. Regeneration</td>
<td>Reduces fuel consumption, as harnessed energy can be redistributed to other consumers when required.</td>
<td>Batteries, flywheels or supercapacitors.</td>
<td>Radan et al., (2016) Bellamy and Bray (2015)</td>
</tr>
</tbody>
</table>

Table 2-4 demonstrates that the three major ESS modes in the field of naval warship power systems are power reserve, load levelling and pulsed load compensation. These are highlighted in bold. These operating modes are the focus of this research. The key technologies considered for integration in the literature are...
Li-ion batteries, supercapacitors and rotating machines or a hybrid combination thereof, which concurs with the conclusions of Bellamy and Bray (2015) and Hehner et al. (2015).

Pulsed power load compensation is considered the primary driver behind the integration of ESS with warship power systems (Lowe et al., 2018; McCoy, 2015; Tate and Rummel, 2017). The possible ESS for this operating mode can be considered from three perspectives. First, if the time duration required to charge the intermediate store between the ships generators and the pulsed load is critical, then power density of the energy store has priority over energy density. Such a system may be very high power single shot capable (e.g. EM railgun or aircraft launch), where the energy store has sufficient capacity for one shot before recharging is required. Consequently, a capacitor or flywheel is likely more suitable than a battery system due to their high power density and cycle life capability attributes (Southall and Ganti, 2018; Wetz et al., 2014). The first perspective has limited feasibility to facilitate all three modes of operation that are of interest in this research for a naval warship, due to their low energy density compared to battery stores. At cell level, battery volumetric energy density is at least six to ten times higher than flywheels and supercapacitors respectively (Chemali et al., 2016), this is important as volumetric constraints are a key factor in naval ship design (Benatmane and Salter, 2018; Kim et al., 2014).

The second perspective is that if the time to recharge is less critical, the power and energy balance of li-ion batteries may be more suitable (Langston et al., 2019; Wetz et al., 2014). The benefit of using a high energy and relatively high power battery is the potential to meet the high energy, high power requirement of the pulse load for a four minute engagement without the need to recharge between pulses. This could alleviate the recharging transients that would otherwise be transmitted the ships generators during the pulse off period. The third perspective, is a hybrid, being the combination of a high power device, e.g. capacitor, and a high energy battery (Mutarraf et al., 2018; Wetz et al., 2018). A hybrid option could mitigate against performance degradation of the battery that could be caused by significant transient loads. Drawbacks of using a hybrid ESS is the requirement for additional power conversion and protection equipment, and a complex power and energy management system that would be required to manage the charge and discharge instances of the power sources under pulsed loading.

This research focuses on the second perspective, using battery technology as the sole ESS. The technological advancements in balancing the power and energy characteristics of battery technology have reached a stage where land-based testing of Li-ion based battery systems has begun for use in pulsed load, load levelling and power reserve applications for warship power systems (Langston et al., 2019). This has accelerated the need to understand how batteries respond to LDEW loading, and their ability to enhance routine operation performance of warships when the LDEW is not required, an investigation that has been absent from the literature, as will be discussed in section 2.5. The recent expression of interest from the U.S Navy to feature Li-ion technology on future warships in the form of an energy magazine (Markle, 2018; Tate and Rummey, 2017) makes this research focus highly relevant to the field.
2.2.8 ESS in parallel industries

This part of the literature review includes parallel industries employing Li-ion based ESS to identify the relevant aspects of knowledge that could be transferred to shipboard ESS. With this as the underlying consideration, two key industries were identified, terrestrial microgrids and the automotive sector. For a detailed review of alternative ESS that are also outside of the scope of the shipping environment the reader is directed to the extensive review by Luo et al., (2015). ESS within that work include large scale pumped hydroelectric storage, liquid air, compressed air, thermal and thermochemical energy. The scale of these methods are suited to land based applications such as energy management for grid systems (Barnes and Levine, 2011).

Terrestrial microgrids

The Li-ion based ESS has been identified as a means to mitigate the reliability and economic challenges imposed by renewable generation sources in microgrids (Alsaidan et al., 2018). The applications include ride through power, power quality (frequency restoration), peak shaving and load levelling (Luo et al., 2015), which bear a heavy resemblance to those summarised above in Table 2-4 for the shipboard environment. With the presence of a battery ESS in the microgrid, the power system benefits from the high energy and fast response characteristics to compensate for the slow response of large-scale power plants and mitigation of renewable source volatility. Terrestrial microgrids often combine fossil-fuel power sources with renewable sources and ESS, where the energy balance between sources and load is managed locally (Bouzid et al., 2015), thus power and energy management with generation units and ESS is essential to guarantee adequate capacity and QPS to meet load demands (Parhizi et al., 2015). Power electronic converters are typically used to integrate renewable sources and ESS with terrestrial microgrids. Since electric ship power systems are essentially an islanded microgrid (Al-Falahi et al., 2018), knowledge of power management methods is applicable to this research. A review of power and energy management is discussed in section 2.3.

Automotive

Li-ion batteries are the predominant ESS technology employed in full electric vehicles (EVs), hybrid and plug-in hybrid road vehicles (Hannan et al., 2018). There has been an improving trend in the development of high energy and power dense cells developed for EVs in the past decade (Kim et al., 2019). These developments are allowing improved vehicle range and reduction in charging time (Hannan et al., 2017). The increasing production demand of EVs is driving down lithium-cell costs through improved economies of scale, technological and manufacturing methods (Hannan et al., 2018; Schmidt et al., 2017). This has led to the marine industry leveraging developments in the automotive sector for safe ESS design (R. D. Geertsma et al., 2017; Watts et al., 2017a). Knowledge of the state-of-the-art characteristics and challenges of Li-ion technology is central to this research. This is reviewed in the section 2.4 to provide the reader with an understanding of the Li-ion technologies that are relevant to the warship environment, and how state-of-the-art developments may provide future benefits to warship power system design and operation.
2.3 A review of power management

2.3.1 Hierarchical power management

As previously mentioned in section 2.2.8, the power management of terrestrial islanded microgrids that combine fossil fuelled generators and ESS are comparable to a ship electrical power system that locally manages the power balance between power sources, and the propulsion, combat system and hotel loads (Al-Falahi et al., 2018; R. D. Geertsma et al., 2017). A key difference between ship based microgrids and land based microgrids are the load and power source dynamics. The dynamics of a typical microgrid load are relatively small, predictable and slow to change. The power sources in microgrids can be intermittent because of renewable sources such as wind and solar. Whereas sources in a ship system are fully controllable and propulsion loads can be highly dynamic (Al-Falahi et al., 2018), therefore ship electric power systems require a robust management system to maintain voltage and frequency QPS.

Power management is commonly categorised into three hierarchical layers as discussed by Guerrero et al. (2011), Bouzid et al. (2015), Gonsoulin et al. (2017), Vu et al. (2017a) and R. D. Geertsma et al. (2017).

The primary layer is local control of the individual power sources, while the secondary and tertiary layers are responsible for system level power management and energy management respectively. This is detailed in Fig. 2-12 for the case of two DGs in parallel with a battery ESS. The following subsections describe the functions of each of the control layers in the hierarchical management structure.

![Diagram of hierarchical power management structure](image)

**Fig. 2-12.** Representative hierarchical power management structure for two DGs and one battery ESS under generic load profile

2.3.2 Primary layer

With reference to Fig. 2-12, in the primary layer the DG governor and Automatic Voltage Regulator (AVR), and the switching devices in the ESS bi-directional power converter are locally controlled.
The AVR of the generator is responsible for controlling the generator terminal voltage to remain constant by adjusting the generated electromotive force (emf) to account for armature resistive losses, armature reaction and the effects of the load power factor on the out of phase power component \((Q)\) of the load. This is achieved by controlling the DC field current applied to the generator field windings. The AVR is responsible for maintaining voltage within the capability of the generator, the stability limits of the power system and to control the generator field to maintain small-signal stability (Kundur, 1994a). The governor of the prime mover, in this case a diesel engine, is responsible for controlling the generator real power, \(P\) and speed. The frequency of the generator voltage is proportional to the speed of the generator and is a function of the number of pole pairs. The frequency of the generator voltage is controlled by adjusting the fuel flow to the diesel engine to control the speed in relation to the load torque. The fuel flow rate governs the energy input to the diesel engine. Some of the energy is retained as rotational inertia while the remainder is transmitted to, or absorbed as rotational inertia from, the load depending upon whether there is a respective increase or decrease in load demand. When a load change occurs, this is reflected in the electrical torque of the generator, consequently causing a differential with the mechanical torque, and therefore results in speed change of the prime mover (Kundur, 1994b).

The primary control layer of the power electronic converter (right of Fig. 2-12) governs the charge and discharge of the battery ESS. The power electronic converter also is responsible for controlling the voltage and frequency of the output AC waveform by controlling the switching pattern of the power electronic devices. At a point in time the switching pattern governs whether the ESS, via the power converter is able to provide active power \((P > 0)\) or store energy \((P < 0)\), or whether the converter and ESS act as a capacitor to supply reactive power to the power system \((Q > 0)\), or as an inductor to absorb reactive power \((Q < 0)\) from the system. The power electronic devices normally have gate turn-off capability and therefore are fully controlled. The most commonly used technique is Pulse Width Modulation (PWM), the duration of the pulses controls the desired voltage and frequency, this is ultimately achieved by comparing a sinusoidal signal for the desired output AC voltage against a triangular carrier waveform. An in depth description of PWM is offered by Rashid (2006).

In the primary control layer a linear frequency and voltage droop characteristic is normally included to prevent the circulation of real and reactive power in the system. The droop characteristic reduces the output frequency and voltage as real and reactive power increases, expressed as:

\[
f = f^* - m(P - P^*)
\]

\[
V = V^* - n(Q - Q^*)
\]

where \(f\) and \(V\) are the frequency and voltage amplitude of the output voltage reference, \(f^*\) and \(V^*\) are the no load output frequency and voltage, \(P\) and \(Q\) are the output real and reactive powers respectively, with \(P^*\) and \(Q^*\) the DG or ESS load set points. \(m\) and \(n\) correspond to the droop slopes for frequency and voltage. The droop values are typically between 3-5% across the full load range of the DG or ESS (DNV GL, 2015; Mills et al., 2018). Fig. 2-13 demonstrates the droop relationship for the ESS. The voltage and
frequency on the distribution system need to be maintained at the desired values which is a control function of the secondary layer.

![Diagram showing P-f and Q-V droop primary, secondary and tertiary control relationships]

Fig. 2.13. P-f and Q-V droop primary, secondary and tertiary control relationships

### 2.3.3 Secondary layer

The secondary layer eliminates the steady state voltage and frequency deviations caused by droop control and proportionally shares power among sources. The secondary layer is also responsible for protective functions and sends start/stop signals to power sources. In a shipboard environment this is typically called a platform management system (R. D. Geertsma et al., 2017). The power set points to the power sources are adjusted by comparing the frequency and voltage error read from the grid through a PI controller. The adjustment signal from the PI controller is sent to the primary layer to decrease or increase the voltage/frequency set point as required within amplitude limits (Bouzid et al., 2015; Guerrero et al., 2011; Han et al., 2017). To avoid interference between the primary and secondary layer controller, the bandwidth is usually one tenth of the primary loop controllers (Vasquez et al., 2010).

Secondary control is categorised by Lu et al. (2014) as centralised and decentralised, the former being conventionally used for ship power systems due to the size of the grid. However, decentralised strategies are also being researched for zonal DC power systems with a high continuity of power requirement.

### 2.3.4 Tertiary layer

The tertiary layer of a land based microgrid manages the active and reactive power flow from the microgrid to the external distribution network by centrally changing the overall voltage and frequency set points of the microgrid (Guerrero et al., 2011). For a shipboard application this would be comparable to the ship being shore connected and operating a generator in parallel with the grid. Alternatively, as discussed by R.D. Geertsma et al. (2017), when the ship is islanded, the tertiary layer in a ship power system manages the allocation of power between the sources by controlling the power set point signals based on the load demand presented. The allocation of power set points to the sources is dependent on the management strategy adopted. These can either be heuristic, logical rule based control strategies, or control strategies that solve an optimisation control objective, for example to minimise fuel consumption (Kalikatzarakis et al., 2018; Ovrum and Bergh, 2015) or generator ramp rates when subjected to dynamic load conditions.
Energy management for naval ship power systems with hybrid power supplies is discussed in further detail in the critical review in section 2.5.3.

### 2.4 A review of battery energy storage systems

The focus of this section will be on the developments in lithium-based chemistries and the pertinent forms for shipboard application. For detailed information on other battery technologies the reader is referred to the texts offered by Linden and Reddy (2010) and Scrosati et al. (2015).

Li-ion battery ESS comprise modules formed of individual secondary (rechargeable) cells, connected in series to achieve a desired voltage or parallel to provide a desired current/power (Fig. 2-14). Modules can be combined to form a string at the desired voltage level, the strings can be subsequently paralleled in a similar fashion to the cells. A battery management system (BMS) is responsible for ensuring the safe and reliable operation of the battery system, to prevent physical damage, manage thermal degradation and cell imbalance (Hannan et al., 2017).

![Maritime battery system schematic](image)

**Fig. 2-14. Maritime battery system schematic**

Battery cells comprise two electrodes and an electrolyte. Each cell can convert between electrical and chemical energy bi-directionally, where the electrodes take the form of an anode (negative electrode) and cathode (positive electrode), whilst the electrolyte can be at solid, liquid or viscous states (Luo et al., 2015). In the discharge state the electrochemical reactions take place at both electrodes simultaneously. From the perspective of the battery system the electrons are transported from the anode and collected at the cathode. Conversely, during the charging state the opposite process takes place by applying an external voltage across the cell electrodes. The pertinent cell characteristics for marine (and automotive) ESS application are energy density and specific energy, power density, safety, cost, cycle life and degradation, charge and discharge rates (Chemali et al., 2016). For ESS design, the battery and thermal management system, and protection are of significant importance (Hannan et al., 2018).
2.4.1 Battery terminology

This subsection provides a description of the battery terminology that will be used throughout this thesis. The characteristics and their descriptions are provided in Table 2-5, these descriptions are made with reference to those detailed in Linden and Reddy (2010).

Table 2-5. Battery terminology

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Description</th>
<th>Unit</th>
</tr>
</thead>
<tbody>
<tr>
<td>Nominal capacity</td>
<td>The total Amp-hours available from the battery when discharged at a specific current discharge condition.</td>
<td>Ah</td>
</tr>
<tr>
<td>State of charge</td>
<td>The battery capacity as a percentage of the maximum capacity.</td>
<td>%</td>
</tr>
<tr>
<td>Depth of discharge</td>
<td>The capacity that has been discharged as a percentage of the maximum capacity.</td>
<td>%</td>
</tr>
<tr>
<td>Open circuit voltage</td>
<td>Terminal voltage of the battery under a no load condition which closely approximates the theoretical voltage of the electrochemical construction of the cell.</td>
<td>V</td>
</tr>
<tr>
<td>Peak voltage</td>
<td>Maximum voltage of the cell at 100% state of charge.</td>
<td>V</td>
</tr>
<tr>
<td>Nominal voltage</td>
<td>Typical operating voltage of the battery.</td>
<td>V</td>
</tr>
<tr>
<td>Cut off voltage</td>
<td>The voltage where the discharge of the battery is terminated. This is the voltage above which most of the battery capacity has been delivered.</td>
<td>V</td>
</tr>
<tr>
<td>C-rate</td>
<td>The current charge or discharge rate in amps as a multiple of the rated capacity of the battery in Ah. At 1C rate the capacity of the battery will be discharged in 1 hour. E.g. for 10 Ah cell, a 1C rate is equivalent to 10 A, 5C is equivalent to 50 A.</td>
<td></td>
</tr>
<tr>
<td>Internal resistance</td>
<td>The sum of the ionic and electronic resistances of the cell components, measured across the cell terminals.</td>
<td>Ω</td>
</tr>
</tbody>
</table>

2.4.2 Lithium-ion cell electrochemistry

This subsection will first compare the characteristics of commercially available cells in different applications, followed by future cell chemistries categorised into near term (0-5 years), medium term (5-10 years) and long term (>10 years). The type of cell is named dependent on the chemical composition of the cathode and/or anode, and this review will consider the pertinent forms. For an extensive review from a chemical technology perspective the reader is directed to Kim et al., (2019) and (Ding et al., 2019). While there are numerous structural and chemical variants of the anode, prioritisation has been placed largely on varying the cathode material (Kim et al., 2019; Wetz et al., 2014). At present, commercially available cells include lithium cobalt oxide (LCO), lithium iron phosphate (LFP), lithium manganese oxide (LMO), lithium nickel manganese cobalt (NMC), lithium titanate (LTO) and lithium nickel cobalt aluminium (NCA) (Benveniste et al., 2018; Hannan et al., 2018).
State-of-the-art cells

LCO has been widely used for consumer power electronics since commercialisation by Sony in 1991 (Kim et al., 2019), the chemistry has a high specific energy characteristic, but at high relative cost due to constrained availability of Cobalt. The cycle life is short at several hundred cycles. The salient disadvantage for shipboard use is the low charge and discharge rates due to the chemical composition. At the elevated temperatures that result from high rates of current charge and discharge, oxygen can be released at elevated temperatures which, in the worst case scenario can lead to thermal runaway (Hannan et al., 2018; Watts et al., 2017a). Conversely, LMO can have better thermal stability and safety when a 3D spinel structure is used but this limits life span, and the energy capacity is approximately 33% lower than LCO (Hannan et al., 2018).

As opposed to LCO and LMO, off grid, marine and EV favour LFP due to its high safety and relatively high power characteristics (Watts et al., 2017a; Wetz et al., 2015), long lifetime, high stability and low cost (Benveniste et al., 2018; Hannan et al., 2018). LFP is regarded as one of the safer chemistries due to high thermal runaway temperature. This is because the strong phosphate bond reduces oxygen release during charge and discharge (Kim et al., 2019). LFP has relatively low specific energy, low voltage (~3–3.6 V per cell) and high self-discharge compared to nickel rich chemistries such as NMC (4.0–4.6 V per cell) (Kim et al., 2019). LCO, LFP, and LMO are considered to be conventional technology due to their relatively low capacity, therefore focus has turned to the development of Lithium and Nickel rich compounds for electric vehicles that benefit from higher capacity and lower cost, particularly NMC cathodes (Kim et al., 2019).

Nickel rich compounds such as NMC attract significant attention from the automotive sector and the commercial marine sector (Radan et al., 2016), particularly due to the high energy/power balance and lower cost through replacing a proportion of cobalt with nickel and manganese in the cathode (Chemali et al., 2016; Kim et al., 2019). The NMC material blend, (typical proportions being NMC-111 or NMC-532 (Ding et al., 2019; Kim et al., 2019)) establishes high specific energy due to nickel, the manganese benefits the thermal stability and low internal resistance (Hannan et al., 2018). The cycle life for NMC is also highly competitive (1,000-2,000) (Benveniste et al., 2018). Conversely, NCA is higher cost, lower safety (Benveniste et al., 2018; Hannan et al., 2018). NCA has a high resistance with cycling and poor behaviour at high C-rates when compared to NMC cells (Wong et al., 2015).

LTO offers benefits in terms of high cycling ability (>5,000), high power, thermal stability, but have low voltage (~2.4 V per cell), low specific energy and very high cost due to titanate (Hannan et al., 2018). The long life and high power capabilities make LTO attractive for stationary and back-up power applications (Stan et al., 2014) where energy density is less of a concern compared to transport applications.

Near term cell chemistries (0-5 years)

Recent interest in the development of NMC chemistries has led to Nickel rich NMC (811) with higher operating voltages (~4.5-4.8 V) to be an attractive option for automotive application (Ding et al., 2019; Kim et al., 2019). The increase of Nickel concentration in the cathode composite contributes to the increased charge capacity, being 25% higher than NMC 111. Compared to NMC-111, NMC-811 also has a higher
electronic conductivity (Ding et al., 2019), this is important for pulsed power applications of any proposed battery chemistry, as this translates to higher C-rate capability. Furthermore, from a material perspective, the reduction in cobalt at the cathode to 10% of the compound, compared to 33% could reduce the initial investment cost of a battery system. The material cost of Cobalt is $29,500 ton⁻¹, Nickel $12,733 ton⁻¹ and Manganese $2,000 ton⁻¹ (London Metal Exchange, 2020), corresponding to a reduction of 9.5% in raw material cost. LG Chem, a large battery cell manufacter, have announced that they will be producing NMC 811 cells in the near future. BMW have also announced their intent to include NMC 811 cells in a production vehicle from 2021 (Ding et al., 2019).

Medium term cell chemistries (5-10 years)

High voltage cathodes (4.5-5.0 V) have been considered as a candidate for the next generation of Li-ion batteries (Kim et al., 2019), particularly high voltage-spinel (HV-spinel). The cathode material of HV-spinel is a compound of Manganese, Nickel and Oxygen, translating to a high specific energy of 580 Wh kg⁻¹. For comparison the theoretical limit of NMC-811 is 350 Wh kg⁻¹. HV-spinel could be of interest for future marine application because it has high rate capability due to high ionic conductivity (Ding et al., 2019). The limitation that needs to be overcome with this particular chemistry is electrolyte decomposition (Kim et al., 2019). The electrolyte properties of a cell play a decisive role in determining how fast a cell can charge or discharge. To improve electrolyte conductivity, separators are a key to unlock enhanced overall ion transport for HV-spinel chemistries (Liu et al., 2019).

Silicon anode technology is considered as an alternative to replace commonly used graphite, due to its high theoretical capacity for lithium, ten times higher than graphite (Ding et al., 2019). However, for high rate applications Silicon anodes are impeded by low conductivity. Further, Silicon anodes suffer drawbacks such as large volume expansion under charging causing structural strain and loss of active components, which limits cycle life.

Long term cell chemistries (>10 years)

The demand for increased range and fast charging in the EV industry has encouraged both industry and researchers to develop the next generation of safe and high energy cells with long cycle life. Cells yet to be commercialised that fall within this remit include lithium sulphur (Li-S), lithium air (Li-air) and solid state batteries (Benveniste et al., 2018; Ding et al., 2019; Kim et al., 2019). Interest in Li-S has arisen due to their very high specific energy density, for example prototype cells have been shown to attain 540 Wh kg⁻¹ (Nagata and Chikusa, 2016). The theoretical energy density of Li-S is approximately 2,600 Wh kg⁻¹ (Benveniste et al., 2018), compared with Li-ion at 410 Wh kg⁻¹ (Linden and Reddy, 2010a). Moreover, Li-S is relatively inexpensive due to replacing the cathode material with sulphur, an abundant element that is low cost (Chemali et al., 2016). However, at present Li-S offers a number of technical challenges hindering commercialisation. They suffer from high volume change of cathode during cycling, low voltage, self-discharge, low ionic conductivity, low coulombic efficiency and low cycle life (Benveniste et al., 2018; Cano et al., 2018).
The Li-air battery has a high theoretical energy density of 3,500 Wh kg\(^{-1}\) and practical energy density of 1,700 Wh kg\(^{-1}\) (Kim et al., 2019). The increased energy density is primarily attributed to the pure metallic anode that can hold more charge than the graphite anodes commonly associated with commercialised electrochemistry (Chemali et al., 2016). Electrolyte degradation leading to poor cycling efficiency and degradation of Li-air batteries currently inhibits their commercialisation.

Organic solvents in conventional Li-ion electrolytes introduce the risk of leakage, fire and explosion during thermal runway events. Therefore solid/gel electrolytes for Li-ion battery applications have been focused upon to improve safety of Li-ion batteries. Solid-state batteries, by definition, do not have a liquid electrolyte. This mitigates against dendrite formation that can short circuit and cause thermal runway in cells with liquid electrolyte. Until recently the inhibiting factors of solid state cells are high internal resistance, low power density (J. G. Kim et al., 2015) due to low-ionic conductivity, and high production cost (Kim et al., 2019). However, recent advances are beginning to unlock the potential of solid electrolytes, by improving energy density and power density (Ding et al., 2019). Advances in solid state electrochemistry are demonstrating discharge rates up to 1500 C, which could be attractive for future pulsed power applications. Flexible packaging is a further advantage as the non-liquid nature of the electrolyte allows the batteries to be packaged to reduce unused volume between cylindrical and pouch type cells, therefore providing higher specific energy. For a warship this could provide a lighter and more space efficient solution.

2.4.3 Cell degradation

The capacity fade during the lifetime of a cell describes the total available charge or discharge capacity while the cell is subjected to its prescribed operating conditions until it has degraded to its end of life (EoL) capacity compared to the beginning of life (BoL) capacity. EoL is defined as the point where total dischargeable capacity reaches no more than 80% of BoL capacity and/or internal 100% resistance rise (de Hoog et al., 2017; Eddahech et al., 2015). The degradation of a cell can be caused by a combination of cyclic or calendar aging conditions. The former includes the operating temperature, depth of discharge, middle SoC range (commonly 90-20% SoC (Hannan et al., 2018)) and number of full equivalent cycles (FEC’s). The latter constitutes storage temperature, SoC and storage time. These conditions influence capacity fade and power loss in the anode by processes such as microcracking, electrolyte decomposition, gas evolution, particle oxidation, dissolution, binder decomposition, surface layer formation and oxidation (Vetter et al., 2005). Conversely the anode and electrolyte interface could suffer graphite exfoliation, electrode cracking, electrolyte decomposition, stabilisation, expansion, lithium plating, dissolution and conversion of SEI layer. For detailed degradation performance the reader is directed to de Hoog et al. (2017) and de Hoog et al. (2018).

From the context of this research, the operational stress conditions that could elevate the degradation rate are operating the cells in the ESS at high current rates and cyclically for pulsed load applications, which inherently increases cell thermal and mechanical stress during operation leading to reduced cycle life. Wetz et al., (2015) demonstrated that when a 3.65 V, 2.4 Ah NCA cell is discharged using a 5s on/1s off duty cycle to a 2.5 V cut off voltage at high rates of discharge, cell EoL was reached at 700 cycles. However,
here the authors subjected the NCA cell to the absolute minimum discharge voltage, whereas operating in the middle SoC range can achieve higher cycle life as demonstrated by de Hoog et al., (2018). When the operating window is limited to within 80-20% SoC, the authors demonstrated that an NMC cell can achieve 2,500 cycles with a 10% capacity decrease under controlled conditions (Table 2-6). It should be noted that this is not the EoL criteria, rather the limit of their investigation.

Table 2-6: Degradation characteristics of a 40 Ah NMC cell from de Hoog et al. (2018, 2017)

<table>
<thead>
<tr>
<th>Resistance increase (%)</th>
<th>Capacity decrease (%)</th>
<th>FEC's to 80% DOD (#)</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>2.5</td>
<td>500</td>
</tr>
<tr>
<td>1.5</td>
<td>4.5</td>
<td>1,000</td>
</tr>
<tr>
<td>7</td>
<td>6</td>
<td>1,500</td>
</tr>
<tr>
<td>16</td>
<td>8</td>
<td>2,000</td>
</tr>
<tr>
<td>19</td>
<td>10</td>
<td>2,500</td>
</tr>
</tbody>
</table>

2.4.4 Comparison of cell characteristics

The pertinent Li-ion battery cell characteristics are compared in Table 2-7. For completeness the characteristics of flywheels and supercapacitors are included. Table 2-7 shows that NMC provides the best balance of characteristics whilst having a relatively high thermal runway temperature, an important characteristic for naval applications. In a similar vein, LFP has a very high thermal runway temperature, however the energy and power density are lower than those for NMC. Whilst NCA and LCO facilitate high energy densities, their thermal runway and cycle life are inferior to those for NMC and LFP. It can be argued that, at present, NMC and LFP electro-chemistries are the more applicable Li-ion battery technologies for ship power systems, therefore they will be discussed further in the critical review section 2.5. Compared to more power dense ESS technologies, batteries have a lower cycle life due to degradation. One of the lowest cost and least advanced technologies is Li-S. If the barriers to cycle life and self-discharge can be overcome, Li-S holds promise for future application in the shipboard environment. Li-air and solid state batteries are at earlier stages of development compared with Li-S, thus, they are not included in Table 2-7.
To establish the extent of the knowledge gap as regards the integration of battery ESS with warship power systems, each investigation in the critical review has been referenced to the three ESS operating modes of interest in this research, namely LDEW power supply, power reserve and load levelling.

### 2.5 Critical review of integrating battery energy storage for dynamic operating modes in warship hybrid power systems

Gattozzi et al. (2015) suggested that laser weapons with transient power requirements will challenge designers to justify an increase in installed prime mover capacity to handle these loads and that an ESS would be required to support them. In assessing Li-ion batteries for laser pulsed power systems using time domain simulation at the University of Texas Austin, Gattozzi et al. (2015) considered three power levels, these being 30 kW, 60 kW and 125 kW optical power, all of which are lower than those anticipated for future LDEWs, as identified in section 2.1.1. Gattozzi et al. (2015) acknowledged that Li-ion batteries could also operate as a power reserve and facilitate load levelling. However, the authors did not investigate the performance benefit of power reserve or load levelling, instead their focus was on comparing minimum volume and mass of LFP based batteries, lead acid batteries, rotating machines and capacitive storage mediums to supply LDEW loads. The authors argue that, operationally, the ESS powering the LDEW, should be connected to the ships power supply when the LDEW is required to be operated, and then the ESS and LDEW disconnected from the ships power supply when the pulsed load is being supplied.

This method was also applied in research conducted by Whitelegg et al., (2015) and Huhman et al., (2016) for EM railgun operation. Gattozzi et al. (2015) further emphasized the importance of the power electronic converter that provides the interface between the ESS, the laser and the load, from a volume, mass and power quality perspective, but offered no discussion regarding the battery simulation model, type of DC/DC converter or the control circuit implemented. The layout of their simulated system is shown in Fig. 2-15.

---

<table>
<thead>
<tr>
<th>Parameter</th>
<th>LCO</th>
<th>LFP</th>
<th>LTO</th>
<th>NMC</th>
<th>NCA</th>
<th>Li-S</th>
<th>Flywheels</th>
<th>Super-capacitors</th>
</tr>
</thead>
<tbody>
<tr>
<td>Energy density (Wh L⁻¹)</td>
<td>450-490</td>
<td>130-500</td>
<td>118-200</td>
<td>230-550</td>
<td>500-670</td>
<td>200-400</td>
<td>20-80</td>
<td>10-30</td>
</tr>
<tr>
<td>Power density (W L⁻¹)</td>
<td>450</td>
<td>200</td>
<td>1400</td>
<td>320</td>
<td>270</td>
<td>990</td>
<td>1,000 – 2000</td>
<td>500-100,000</td>
</tr>
<tr>
<td>Specific energy density</td>
<td>170-240</td>
<td>80-140</td>
<td>85</td>
<td>126-220</td>
<td>145-240</td>
<td>200-550</td>
<td>10-30</td>
<td>1-10</td>
</tr>
<tr>
<td>(Wh kg⁻¹)</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Cycle life (cycles)</td>
<td>500-700</td>
<td>1,000-2,000</td>
<td>12,000</td>
<td>1,200-2,000</td>
<td>500</td>
<td>~50</td>
<td>1,000,000</td>
<td>1,000,000</td>
</tr>
<tr>
<td>Cost (US$/Wh)</td>
<td>0.31-0.46</td>
<td>0.3-1.0</td>
<td>1.0-1.7</td>
<td>0.3-0.9</td>
<td>0.23-0.61</td>
<td>0.25</td>
<td>5</td>
<td>0.3-2</td>
</tr>
<tr>
<td>Self-discharge rate (per month)</td>
<td>1%</td>
<td>1%</td>
<td>1%</td>
<td>1%</td>
<td>1%</td>
<td>8-15%</td>
<td>100%</td>
<td>100%</td>
</tr>
<tr>
<td>Thermal runway (°C)</td>
<td>150</td>
<td>270</td>
<td>n/a</td>
<td>210</td>
<td>150</td>
<td>125-200</td>
<td>n/a</td>
<td>n/a</td>
</tr>
</tbody>
</table>

---

**Table 2-7: ESS characteristic comparison (Benveniste et al., 2018; Chemali et al., 2016; Hannan et al., 2017; Kim et al., 2019; Nagata and Chikusa, 2016; Watts et al., 2017a)**
The 1,000 V battery strings in their work consisted of 270 series connected LFP cells. The authors then found that for the 125 kW laser, a minimum of 2 strings were required to facilitate sixty, 6-second shots at 50% duty cycle.

![Diagram](image)

**Fig. 2-15. Proposed battery and laser interface with DDG 51 distribution system (Gattozzi et al., 2015)**

Perhaps the most comprehensive series of publications into battery pulsed loading has been borne out of a collaborative effort by the University of Texas at Arlington and the U.S. Naval Research Laboratory (Huhman et al., 2016; Huhman and Neri, 2013; Huhman and Wetz, 2015). Here the authors detailed the design and development of a hybrid ESS approach to mitigate against EM railgun load transients impacting the ship power systems directly. Their approach was to use a laboratory demonstrator, where an LFP based battery charges a capacitive pulsed power circuit (Fig. 2-16) at 12 kJ/s to 5 kV in 5s before a shot is taken. While the on-time is 5s, a comparable duration with anticipated LDEW loads, the capacitor load off-time from the perspective of the battery is 10 ms as the shot is taken. The relevant contributions from the referenced publications that apply to this research pertain to the battery characteristics under pulsed discharge and the design of the DC-DC power converter.

The 600 V battery in Fig. 2-16 comprises twelve 50 V LFP modules, with sixteen 3.2 V, 2.6 Ah cylindrical 26650 form factor (26 mm diameter, 650 mm height) cells per module. While the energy capacity is low, the authors justify the selection of LFP on the basis of the thermal runway temperature. Under such cyclic loading, the authors emphasize the importance of first maintaining battery temperature below a 55°C limit to ensure safety. Second, the importance of cycle life and degradation, and third the importance of cell balancing. The battery charges the capacitor via a high frequency, 20 kHz H-Bridge DC-DC converter with intermediate parallel resonant circuit and step up transformer. The converter circuit boosts the voltage from the battery nominal 600 V DC to the required 5 kV level required for the capacitor that ultimately drives the load. The high frequency characteristic minimises the size of magnetic components in the circuit and allows a high boost voltage ratio but this involves high switching losses. A description of the converter is provided in Huhman and Neri (2012), however, similar to Gattozzi et al. (2015), discussion of how the DC/DC converter was controlled to provide the pulsed load was not included in their publications. The authors did not discuss secondary operating modes of the battery, such as the power reserve or load levelling mode.
Hebner et al. (2015) concurred with Gattozzi et al. (2015), emphasising the importance of the ESS in a multi-role capacity for pulsed power supply, providing the ability to continue to supply part or all of the load when a generator set goes offline and providing power during step load changes. Hebner et al., (2015) use the concept of an energy magazine to facilitate these three modes, describing the energy magazine as “the energy stored to power pulsed loads when needed and to enhance routine operations when possible”. The U.S Navy introduced the energy magazine concept in 2013 (Kuseian, 2013) (see Fig. 2-17), and expanded on this in 2015 to be a trade space for energy storage, power electronic converters and auxiliary systems (Kuseian, 2015), as part of their technology development plan for future ships from 2021.

In addressing the concept of an energy magazine, Tate and Rumney (2017) and Langston et al. (2017) describe a 320 kW, 3.6 MJ flywheel ESS being investigated using power hardware-in-the-loop (PHIL) simulation experiments conducted at Florida State University, converse to the purely computational simulation based approach used by Gattozzi et al., (2015). The pulse load applied in their experiment was 130 kW, at 0.01 Hz repetition rate and 15% duty cycle, all characteristics lower than the anticipated load profile described in section 2.1.1. However, the aim of this work was to commission the PHIL system for further transient testing. The flywheel hardware tested in Langston et al. (2017) was tested further at the Power Networks Demonstration Centre at the University of Strathclyde. Jennett et al. (2019) discuss the testing of the same flywheel hardware, however with an updated control system that includes a feedforward delay compensation to counter the flywheel ESS response delay to charge and discharge. This body of work is aimed at de-risking energy storage options for integration with LDEW pulsating loads for the US Department of Defence and the UK. Jennett et al. (2019) did not present results of the response of the FESS when supplying LDEW loads, however their paper did discuss the ambition to interface the FESS with ship power system simulation models to understand the performance of the power system over different operating scenarios in future work.
Continuing the research focus on the energy magazine at Florida State University, Langston et al. (2019) describe PHIL testing of a 1,000 V, 660 kW, 200 MJ Li-ion battery. Their aim being to present results of the commissioning and testing of the battery when supplying periodic and stochastic pulsating loads. This contributes to the de-risking effort of integrating ESS with warship power systems to supply pulsed loads. This contribution highlights that there is a research need to investigate the ability of battery technology to supply pulse loads for a warship.

Tate and Rumney (2017) also concluded that architecturally, the selection of the energy store and how distributed this needs to be within warship power systems remains a challenge. It should be noted that if the ESS is distributed, in the presence of pulsed loads, the system inductance will present a challenge to the high rate of change of current, meaning that shorter, lower inductance connections to the pulsed load are best able to meet the pulse characteristics (Hebner et al., 2015). However, distributed storage would potentially improve survivability of the power system at the cost of increased complexity. This was highlighted previously by Newell et al. (1999) who emphasized the importance of locating the energy store within the proximity of pulsed weapons due to high power flow characteristic.

A prevalent research topic in the area of ESS integration for pulsed power is energy management. An important series of works on this topic have been contributed from Florida State University. T. V. Vu et al., (2017a, 2017b) details a proposed energy management scheme for MVDC power systems that aims to provide sharing of power among GT generators and ESS. The energy management schemes allocate power set points to the ESS and generators to ensure that the demands of EM railgun and high ramp rate loads are met whilst simultaneously satisfying ramp rate limits, bus voltage and stability constraints. The approach taken was to implement a model predictive control method in the energy management layer of a hierarchical control system (similar to that shown in Fig. 2-12 in section 2.3.1) that controls the power electronic interfaces of the power sources. Model predictive control is an optimisation-based control strategy that aims to minimise a performance criterion of the controller response over a future horizon. This was conducted using a HIL test bed that represents a 1:10,000 scale of a 60 MW MVDC power system, similar to that described in Fig. 2-2. The novel methodology reduces bus voltage transients during EM railgun loading from 4% to 0.5%. The ESS in the HIL system is represented using a battery emulator. The authors do not discuss the type of battery emulated. The authors do not detail whether the emulated ESS in their study supports the EM railgun or if there is a local energy store that drives the railgun, similar to Huhman et al. (2016). The plots in their publication would indicate the latter as the load duration is in the order of seconds.

Building upon their previous work at Florida State University, Gonsoulin et al. (2018) make steps to integrate distributed power and energy management into the power system design process. One contribution beyond the work described in T. V. Vu et al., (2017a, 2017b) is a power reserve methodology that maintains SoC in the ESS when the ESS is not being used for ramp rate compensation of highly transient loads. The real-time simulator used ideal current and voltage sources to represent sources and loads as opposed to using power electronic converters that would be present in an actual MVDC power system. The short case study contributes to the field by varying the amount of energy stored in the ESS from 25 to 75 MJ and the ramp rate from 1 MW/s to 2 MW/s, in order to consider the effects this has on the generator response and depth of discharge of the ESS under transient load conditions where the ramp rate is relatively high. As
would be expected the methodology showed that the ESS capacity has a direct effect on generator response to a load ramp transient. The second contribution of this publication is devising a method of comparing the characteristics of an energy store for the case study system to achieve the desired power system response under EM railgun loading.

Similar to Gonsoulin et al. (2018), Rashkin et al., (2018b) from Sandia National Laboratories used advanced control design techniques in a 1:100 scale electric ship system to conduct an investigation into ESS sizing for warship power system design. All components in their system interact with the grid through power electronic converters and a hierarchical control structure. Unlike the model predictive control strategy in T. V. Vu et al., (2017a, 2017b) that optimised ramp rate demand to determine the power split between ESS and generators, Rashkin et al. (2018b) vary the balance of the power provided by either an ESS or generator by varying the time constant of a first order filter in the control structure. This filter forms part of the control algorithm that shares current and voltage measurements between the power electronic converters. Under varying load demand, the longer the time constant the greater the requirement for the energy store to increase its supply contribution to the load, while the shorter the time constant the greater the requirement for the generator to increase its supply contribution to the load. The author applied a mission profile while varying the time constant of the filter to investigate ESS sizing. This was done by post-processing time-domain results and mapping the total energy and maximum power from the simulation to a Ragone plot (W kg$^{-1}$ versus Wh kg$^{-1}$). This contribution allows the designer to establish the range of ESS technologies that may be appropriate for the system under test for a specific operating profile. The authors conclude by stating that although this process may identify an appropriate storage technology, the selected technology still needs to be analysed with respect to its system response. A summary of the types of ESS technology that have been investigated for warship pulse power application is provided in Table 2-8.

Table 2-8. Summary of ESS technology investigated for warship pulse power application

<table>
<thead>
<tr>
<th>Reference</th>
<th>ESS type</th>
</tr>
</thead>
<tbody>
<tr>
<td>Gattozzi et al. (2015)</td>
<td>LFP based battery, lead acid battery, rotating machines, capacitors</td>
</tr>
<tr>
<td>Whitelegg et al. (2015)</td>
<td>Capacitors</td>
</tr>
<tr>
<td>Huhman et al. (2016), Huhman et al. (2013) and Huhman and Wetz (2015)</td>
<td>Hybrid – LFP battery system and capacitors</td>
</tr>
<tr>
<td>Tate and Rumney (2017), Langston et al. (2017) and Jennett et al. (2019)</td>
<td>Flywheels</td>
</tr>
<tr>
<td>Langston et al. (2019)</td>
<td>Li-ion battery system (specific chemistry not specified)</td>
</tr>
<tr>
<td>T. V. Vu et al., (2017a, 2017b)</td>
<td>ESS type not specified</td>
</tr>
</tbody>
</table>

2.5.2 Power reserve operating mode

Prospective power system performance improvements when an ESS operates as power reserve and load levelling are dependent on the roles of the warship, operating profile, the power system configuration and the energy management strategy (R. D. Geertsma et al., 2017; Georgescu et al., 2018).

Kim et al. (2014) at Seoul National University proposed the integration of an LFP based ESS to provide ride through power to a proposed IFEP system (Fig. 2-18) when connected to the DC link of the propulsion converter. The proposed full scale battery ESS in their work consisted of 28 series connected LFP modules
to achieve a single 128 kWh string with a voltage range of between 1,027 V and 1,220 V that matched the DC link voltage of the propulsion converter to which the string was directly connected. The authors acknowledged that utilising the propulsion converter DC link could reduce the need for additional converters and their associated auxiliaries, thus reducing the integration footprint, which is desirable for warships with volumetric constraints (Kim et al., 2014). However, this requires oversizing the active front end (AFE) rectifier of the propulsion converter to permit simultaneous battery charging and delivery of propulsive power. Moreover, controlling the charge/discharge of the battery whilst maintaining a stable DC link voltage can pose a technical challenge (Radan et al., 2016), exacerbated when a current pulse with high di/dt induces battery voltage drop, such as an LDEW load. The study by Kim et al. (2014) made efforts to address the control of an AFE using simulation and HIL testing. In their work the ESS has four operating modes, to supply the propulsion motors under normal operation, to support the grid under a DG trip event or pulsed load discharge, and absorb regenerative energy from the propeller during ship deceleration.

Fig. 2-18. Kim et al., (2014) proposed electric power system with battery ESS on the propulsion converter DC link

A diagram of the HIL test rig used by Kim et al., (2014) is shown in Fig. 2-19. The tests were conducted at 1:3.45 voltage and 1:1,000 power scale, where the generators, ship service resistive inductive load and pulsed load are simulated, the AFE inverter and rectifier and ESS were the hardware in the loop under test, and the propulsion load was implemented as a generator under load.

Fig. 2-19. Kim et al., (2014) battery ESS hardware in the loop testing
The authors demonstrated the effectiveness of the controller to utilise the battery energy for a period of 3 minutes following the trip of the smaller DG. The aim of the controller is to prevent frequency fluctuation at the distribution bus by sending a current command to the AFE controller to either charge or discharge the battery from/to the ship’s grid respectively. While the results showed that frequency QPS was maintained during a DG trip event, their work lacked accurate battery representation during the HIL testing as the battery capacity was oversized. Oversizing the battery reduces the voltage variation for the same amount of energy drawn from the battery. This implication restricts the confidence in the measured response of the AFE DC link voltage under transient conditions.

Building upon their previous work, Kim et al. (2015) presented results of a steady state case study into the potential fuel consumption savings when the battery ESS capacity acts as a power reserve for the ship power system shown in Fig. 2-18. Their method considers the safe loading factor on each operating generator to ensure continuity of power supply to the ship’s loads should there be an event that renders one generator inoperable. Under a presumed operating profile, the authors highlighted that a 2 MW, 500 kWh ESS could reduce fuel consumption by 4%. The authors accepted that the configuration of the power system and operating profile will impact these results. This is a limitation of their work. Both publications did not present results on load levelling or peak shaving.

A second investigation described in the publication by S.-Y. Kim et al. (2015) expanded on the HIL test results presented in Kim et al. (2014) to include a pulsed load supplied from the main distribution bus via a transformer and diode rectifier. The pulse load was of trapezoidal shape with 20 ms rise time and 80 ms fall time and 200 ms pulse duration, peaking at 5 MW, this represents a significant proportion of the total installed power (7 MW). Results were presented for a single pulsed load shot, and the results indicated that their control strategy presented in Kim et al. (2014), was able to limit the bus transients within an allowable ± 3 % frequency deviation and ± 5 % voltage deviation. However, the authors disclosed that the physical limitations of the battery ESS were ignored in their work, therefore caution should be exercised when reviewing their results.

The reviewed publications for power reserve mode thus far have been applicable to warship power systems, however, there have been a number of important publications in the commercial marine sectors on the integration of battery ESS whose contributions have transferrable elements when an ESS operates as power reserve on warships. Commensurate with the similar explanation of power reserve, S.-Y. Kim et al. (2015), and Radan et al. (2016) from General Electric describe the same power reserve relationship mathematically, and state that the ESS is required to provide the power for a period of 3 to 5 minutes. Although Radan et al. (2016) did not apply the power reserve concept to a warship power system case study, the concept was applied to an offshore supply vessel, which, when operating in DP mode, can have comparable power system resilience requirements of a warship (except when at action stations) to maintain safe operation. The case study power system included four 2.25 MW DGs. The authors compared the fuel consumption saving when integrating a 1.1 MW and 1.4 MW NMC battery ESS. Over the operating profile the authors showed that fuel consumption could be reduced by 5.4% and 6.3% for the respective ESS powers, while the reported running hour reduction was 23.3% and 27.2% respectively.
Hodge and Mattick (1999) originally emphasised the importance of determining when an energy store can or cannot be used as the power reserve mode during single generator operation. The authors cited conditions such as operating in restricted waters, Replenishment at Sea (RAS) and special sea duties (e.g. operating evolutions such as helicopter take-off/landing)/bad weather. This has been absent from the work of S.-Y. Kim et al., (2015) and Radan et al. (2016) when considering a battery energy store for the power reserve mode.

2.5.3 Load levelling and peak shaving operating modes

In a simulation based investigation, Kalikatzarakis et al. (2018) from Delft University present results of their work on energy management strategies that aim to minimise fuel consumption of a tug boat when an ESS is used for load levelling. The tug boat employs a hybrid power and hybrid propulsion system, and the ESS was based on lithium iron magnesium phosphate. Kalikatzarakis et al. (2018) contribute a simulation study that comprises validated high fidelity, engine, generator and battery models with their respective characteristic efficiency curves. Their investigation results show how optimal energy management strategies, when compared to rule-based control under load levelling, can improve fuel consumption by up to 19.5% in some operating modes. The shortcoming of their approach is prior knowledge of the operating profile is required. The performance of the electrical system was outside the scope of their work.

The approach by Dinh et al. (2018) from Warwick Manufacturing Group was to use a HIL simulation to test energy management systems for a half ship set representation of a platform supply vessel with battery ESS as described by Fig. 2-20. The purpose of the battery in their work was to load level the two DGs as required during five operating states; harbour, harbour loading, transit, DP loading and DP standby over a 17.5 hour profile.

Dinh et al. (2018) developed a mathematical model to represent a marine hybrid power system and implemented three energy management strategies to manage power flow between sources, load and ESS and compare the fuel consumption against a baseline vessel not fitted with a battery ESS. The rule based control strategy implemented was reported to save 2.44% in fuel consumption over the baseline power system. However, the key contribution of this publication is the implementation of an equivalent fuel consumption minimisation (optimisation) strategy and second optimal control strategy that improved fuel savings to 5.2% and 5.8% respectively. For the operating profile tested, a shortcoming of the implemented equivalent fuel consumption minimisation control strategy was the increase in generator start/stop count for one generator from 3 to 17 for the optimal control strategies.
There were also some intrinsic limitations to their study. The authors did not include the transient response of power electronics and generators. The losses for all devices except the generator were assumed constant and fixed. Furthermore, the battery model was simplified to a DC voltage source represented as an open circuit voltage and equivalent series resistance.

Watts et al. (2019) expanded on the previous work in Dinh et al. (2018), improving the battery model fidelity by including test data from a 43V, 90 Ah module in the HIL test rig. Their approach compared to Dinh et al. (2018) was enhanced by using a modified rule based control strategy, whose aim was to ensure the engines operate at their optimal loading condition. The simulations were run over the same operating profile described by Dinh et al. (2018). This time a 4.6% saving was reported, with one generator running constantly and the second having 10 start/stops. This can be compared with 3 and 29 start/stops under the rule based control strategy in Dinh et al. (2018).

Bordin and Mo (2019) describe the addition of an optimisation tool into the sizing of the energy storage cognisant of operating constraints applied to the system summarised in Fig. 2-21.

![Fig. 2-21. Power system studied in (Bordin and Mo, 2019)](image)

The modes of operation included a low resilience mode where blackout is permissible, minimum one DG, battery only, one DG operating on each switchboard, one DG on each switchboard with the battery permitted to act as power reserve. Although useful as an investment analysis tool, the operating modes were not representative of warship modes of operation which require high resilience as discussed by Allen and Buckingham (2017), Hebner et al. (2010), Mahoney et al. (2012) and Southall and Ganti (2018). Limitations of their work were model fidelity, and the battery and generator efficiencies were assumed constant. Moreover, the converter and transformer efficiency characteristics, which were not taken into account, in reality vary over the ESS operating envelope during charge and discharge. The battery type was not discussed in their work. Importantly however, similar to the conclusion of Kalikatzarakis et al. (2018), Bordin and Mo (2019) concluded that the mode of operation has a noteworthy influence on fuel consumption.

While collectively, the previous publications by S.-Y. Kim et al. (2015), Kalikatzarakis et al. (2018), Dinh et al. (2018), Watts et al. (2019) and Bordin and Mo (2019) concurred that performance improvement in terms of fuel consumption and emission reduction could be achieved by integrating a battery ESS. There is a notable absence of discussion or rationale regarding the siting of the ESS within the power system.
architecture. In addition, there is a lack of discussion regarding the specification of the ESS when intended to both power future pulsed loads and support secondary operating modes (power reserve and load levelling). First, this is important in this research as the location of the ESS can influence the ability to operate the pulsed load whilst power is maintained to propulsion, without the possibility of interruption to ensure the ship maintains the ability to both move and fight. Second, the location of the ESS influences the scale of the conversion losses, thus influences the system efficiency and cost.

2.6 Summary

This chapter has demonstrated that Li-ion battery ESS integration with ship power distribution systems have developed to a junction where their integration with warship power systems is warranted for certain applications and is therefore an important field of applied research.

The first part of this literature review aimed to develop an understanding of the operating modes of a battery ESS in commercial and naval ship electric power systems, and the performance benefit that this can introduce to a ship power system. This was proceeded by developing an understanding of how the power is managed between generators and battery ESS in hybrid power systems, before informing the reader of the state-of-the-art in battery technology. A summary of the key developments these three sections of the literature review provided is as follows:

1) ESS operating modes in warship power systems include power reserve, load levelling and peak shaving, pulsed power load compensation, regeneration storage and acting as a sole power source (Hebner et al., 2015; Southall and Ganti, 2018; Zohrabi et al., 2019). A summary of the corresponding characteristics for each ESS mode and the possible technologies in the reviewed literature is provided in Fig. 2-22.
2) Pulsed power load compensation is the driving factor for change in power system design and the primary requirement for ESS (Lowe et al., 2018; McCoy, 2015). For IFEP ships the dominant pulsed power load is anticipated to be the EM railgun (Hebner et al., 2015). Conversely for warships with hybrid power and propulsion the dominant pulsed load is the LDEW (Mills et al., 2018).

3) The wide contrast between the prime mover and ESS characteristics requires a robust power management system to ensure that the power delivery balance of the sources is properly managed. This is achieved using a three tier, hierarchical power management system (R. D. Geertsma et al., 2017; Vu et al., 2017a).

4) While there is an array of Li-ion battery technologies available that are under continuous development due to the investment in the automotive sector (Kim et al., 2019), two key battery technologies presently exist that are of interest for integration with ship electric power systems. These are LFP and NMC based chemistries, the former from a safety standpoint and the latter providing a balance of safety, power and energy (Chemali et al., 2016; Kim et al., 2019).

As a consequence of a critical review of published research on the use of battery ESS for dynamic load operating modes in warship power systems, it is acknowledged that there have been important contributions to the field. These are summarised as follows:

1) **Pulsed power load compensation.** Battery energy storage has been identified as a technology to power LDEW and undertake secondary operating modes to improve power system
performance when the LDEW is not required. Simulation based research has demonstrated that batteries could be capable of facilitating such loads (Gattozzi et al., 2015). However, the ESS is required to facilitate a four minute LDEW engagement (Markle, 2018) and analysis of the ability of a battery ESS to facilitate this has been absent. Furthermore, the battery model, power electronic interface and control system have not been presented in sufficient detail to discuss the battery limitations during operation of an LDEW.

2) Advanced control systems in the energy management layer of hierarchical control systems have been demonstrated to improve power system QPS under pulsed loading (Vu et al., 2017b, 2017a) and could be used as a method to inform ESS sizing (Gonsoulin et al., 2018; Rashkin et al., 2018b). However, analysis to demonstrate that the ESS technology can facilitate pulsed power duty cycles representative of an LDEW has been limited, and absent when the ESS is used for both power reserve and load levelling modes.

3) **Power Reserve.** When ESS is integrated with the power system, fuel and exhaust GHG reduction can be achieved by increasing the generator safe loading margins. Fuel consumption reduction of 5-7% is achievable on the basis of using small ESS in the power reserve mode (S. Y. Kim et al., 2015; Radan et al., 2016). Previous studies have not discussed the power system configurations in sufficient detail to accurately quantify the potential savings. It has been demonstrated that a battery ESS can provide ride through continuity of power supply during a generator trip event whilst maintaining QPS (S. Y. Kim et al., 2015).

4) **Load levelling.** Various instances have been reported of the ability of optimisation energy management strategies to reduce fuel consumption when compared to rule based control. Unlike literature on ESS in the power reserve mode, operating states have been considered for load levelling, where key contributions have been made by Kalikatzarakis et al. (2018), Dinh et al., (2018), Watts et al. (2019) and Bordin and Mo (2019).

5) **Battery technology.** Fig. 2-23 presents a summary of Li-ion battery technology development as discussed in section 2.4 and the ESS technology highlighted from contributions in this literature review. Nickel rich, NMC-811 and higher voltage cathode materials will develop further in the near future, followed by HV-spinel in the medium term, before Li-S, solid state and Li-Air batteries can penetrate their respective technological barriers for commercialisation (Kim et al., 2019). LFP has had various instances of research published for consideration in warship power system application. However, this has not been the case for NMC, which has received greater attention in commercial marine and parallel sectors. NMC in shipping is warranted due to its balance of power/energy and safety characteristics, therefore it is of high relevance to the field, and needs further investigation to understand the benefits and limitations of its application to warship power systems.
Fig. 2-23. Historical evolution of pertinent Li-ion technologies for shipboard application

Much of the existing research in the field has considered the power reserve, load levelling and pulsed load supply modes in isolation. For the power reserve mode, previous research has often neglected the efficiency characteristic of the energy store and associated interface with the power system, and just as importantly, the power system configuration during operating modes. Under the load levelling mode, electrical performance has often been neglected in energy management studies. Where previous research has considered battery ESS for a candidate warship system to facilitate LDEW load compensation, limited attention has been placed on the power electronic interface and associated control system. Importantly, continuous firing of a battery ESS under LDEW loading to operate for four minutes has not yet been demonstrated by a battery based ESS. Consequently, there are three ways in which this field of research may be advanced further and is summarised as follows:

1) Steady state model with which to explore a candidate warship power system performance when a battery ESS is operating as power reserve. The model should include operational profile, power system operating configuration and power source efficiency characteristics. The model should include the ability to evaluate engine fuel consumption, exhaust GHG emissions and engine running hours.

2) Quasi steady state model with which to explore a candidate warship power system electrical performance when a battery ESS is operating to load level generator sets. The model should include the ability to evaluate fuel consumption, exhaust GHG emissions and QPS performance.

3) Design and development of a battery based LDEW power system model including converter and control system capable of facilitating continuous firing to achieve an engagement target of four minutes whilst maintaining QPS. The operational constraints that the battery must be
restricted to in order to achieve the LDEW four minute engagement length at BoL and EoL should also be defined.

A timeline summary of the key research published in the field is provided Fig. 2-24 to conclude this chapter. This timeline demonstrates the relevance in advancing knowledge in the field, the publications resulting from the research presented in this thesis, acknowledged in chapter 1, are also included in the timeline.
---|---|---|---|---|---|---
**2013**
Huhman, Wetz and Mili
Naval Research Laboratory, University of Texas at Arlington and Vagena Tech
Development of a Rep-Rated Pulsed Power System Utilizing Electrochemical Prime Power

**2014**
Gamozi et al.
University of Texas Austin and Naval Postgraduate School
Power system and energy storage models for laser integration on naval platforms

**2015**
Kim et al.
Seoul National University
Electric Propulsion Naval Ships with Energy Storage Modules through AFE Converters

**2016**
Huhman and Neri
Naval Research Laboratory
Investigations into the Design of a Compact Battery-Powered Rep-Rate Capacitor Charger

**2017**
Kim et al.
Republic of Korea Naval Academy, Seoul National University and Central Research Institute of Samsung Heavy Industries
A Naval Integrated Power System with a Battery Energy Storage System: Fuel efficiency, reliability, and quality of power

**2018**
Langston et al.
Florida State University
Power Hardware-in-the-loop Simulation Testing of a Flywheel Energy Storage System for Shipboard Applications

**2019**
Kalitkarzaki et al.
Delft University of Technology
Ship Energy management for hybrid propulsion and power supply with shore charging

**2014**
Farrier et al.
University College London
Simulating Pulsed Power Load Compensation Using Lithium-ion Battery Systems

**2015**
Langston et al.
Florida State University
Power Hardware-in-the-loop Simulation Testing of a 200 MJ Battery-Based Energy Storage System for Shipboard Applications

**2016**
Kalitkarzaki et al.
Delft University of Technology
Ship Energy management for hybrid propulsion and power supply with shore charging

**2017**
Watts et al.
Babcock and Warwick Manufacturing Group
Aguile power management systems – A rule based control strategy using real-time Simulation for hybrid marine power plants

**2018**
Rashkin et al.
Sandia National Laboratories
Energy Storage Design Considerations For an MVDC Power System

**2019**
Langston et al.
Florida State University
Power Hardware-in-the-loop Simulation Testing of a 200 MJ Battery-Based Energy Storage System for Shipboard Applications

**Timeline of key published research on the integration of battery ESS with ship power system**

Chapter 3   Problem formulation

3.1 Introduction

The aim of this chapter is to provide a conceptual understanding of how a battery based ESS is integrated with a candidate power system and how the ESS operates to power LDEWs, acts as a power reserve and load levels generator sets. It is therefore necessary to select and justify a candidate warship power system and the power system operating conditions to define a baseline with which to assess power system performance when a battery ESS is integrated. As such this chapter provides context and analysis of the problem presented by electrically integrating a battery ESS with a warship power system to meet the three contrasting ESS operating modes of powering an LDEW, power reserve and load levelling generator sets. Therefore having done so, determine how power is managed between power sources to meet the performance demands of each ESS operating mode. Hence, the research problem is formulated as follows:

1) Identify a suitable candidate warship electric power system with which to integrate a battery based ESS, detail the operating profiles, operating states and corresponding power system operating configurations, and identify the battery ESS operating mode in each operating state.

2) Describe the concept of operation of an LDEW, identifying the key system performance parameters.

3) Define the battery ESS operating mode requirements, battery ESS constraints in the context of ship design and assess how the location of a battery ESS within the power system architecture could impact power system performance, and through this recommend the integration location within the candidate warship power system.

4) Mathematically explore the specification of the battery ESS and interface design with suitable characteristics to match the ESS operating mode requirements for the candidate warship power system.

5) Identify the power management relationships when the ESS is operating in each mode and explore how sources share power delivery to meet the load demand.

6) Identify the pertinent performance criteria to explore the impact of each battery ESS operating mode.
3.2 Candidate ship and modes of operation

As was stated in chapter 2, pulsed loads are a key driver of change in power system design (Lowe et al., 2018) and for hybrid power and propulsion systems LDEW are anticipated to be the dominant pulsed loads (Mills et al., 2018). Thus, this research has focused on hybrid power and propulsion systems and powering LDEW, as the future Type 26 frigate that comprises such a power and propulsion system, has been recognised as a platform to field LDEW in the future (Ministry of Defence, 2019). The candidate warship is a notional modern frigate, the characteristics for which are derived from Gemmell et al. (2014), as outlined in Table 3-1. Fig. 3-1 shows a simplified power system diagram commensurate with those suggested by McNaughtan et al. (2016) and Mills et al. (2018) for warship hybrid power and propulsion systems. The system is a combined diesel electric or gas turbine (CODLOG) topology with two shafts driving fixed pitch propellers. The system in Fig. 3-1 incorporates four 3 MW high-speed diesel generators (DGs) as the primary source of power connected to two main switchboards rated at 690 V. Two feeders supply the ship service switchboards via 690/440 V step-down transformers. In electric drive mode, propulsive power is drawn from the main switchboard via propulsion converters to two shaft-mounted motors aft of the gearbox. Mechanical ‘boost’ propulsion is provided by a 36 MW gas turbine (GT) driving the twin shafts via a cross-connect gearbox to achieve high speed. Summary for the justification of the warship power system components outlined in Fig. 3-1 is provided in Table 3-2.

<table>
<thead>
<tr>
<th>Ship Characteristic</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Displacement</td>
<td>6,200 tonnes</td>
</tr>
<tr>
<td>Length overall/length waterline</td>
<td>143/130 m</td>
</tr>
<tr>
<td>Overall Beam</td>
<td>17.2 m</td>
</tr>
<tr>
<td>Top Speed</td>
<td>&gt;26 kts</td>
</tr>
<tr>
<td>Economical speed</td>
<td>12 kts</td>
</tr>
<tr>
<td>Temperate hotel load</td>
<td>2.5 MW</td>
</tr>
<tr>
<td>Shaft power at economic speed</td>
<td>2.3 MW</td>
</tr>
<tr>
<td>Crew</td>
<td>140-160</td>
</tr>
</tbody>
</table>
Fig. 3-1. Candidate warship power and propulsion system

Table 3-2: Summary for the justification for the selection of power system components

<table>
<thead>
<tr>
<th>System aspect</th>
<th>Justification for selection</th>
</tr>
</thead>
<tbody>
<tr>
<td>Hybrid power and propulsion architecture</td>
<td>LDEW are anticipated to be fielded on warships with hybrid power and propulsion in the near future (Defence Science and Technology Laboratory, 2019; Mills et al., 2018).</td>
</tr>
<tr>
<td>AC radial distribution</td>
<td>AC radial distribution systems are typically included on warships. Examples included the Royal Navy (RN) Type 26 and Type 45.</td>
</tr>
<tr>
<td>High speed 3 MW DG</td>
<td>High speed DGs are typical power generation for hybrid power system architectures including the RN Type 23 and Type 26, as high speed permits a smaller DG set. Commensurate with those investigated in Mills et al. (2018).</td>
</tr>
<tr>
<td>3.4 MW Induction propulsion motor</td>
<td>Based upon the propulsive requirement of the candidate ship to achieve 16 knots, after which the GT provides mechanical boost propulsion power.</td>
</tr>
</tbody>
</table>

3.2.1 Operating profile, states and configurations

In this work, operation of the candidate warship and its electric power system is broken down into four key elements. The first element is the operating profile, which defines the percentage of total operating time that the ship spends in an operating state per year. The second element, the operating states of the candidate ship, include manoeuvring, patrol, transit, replenishment at sea (RAS), high speed cruise and State 1 (Action Stations) operating states. The third element is the operational configuration of the electric power system during each operating state. This element pertains to the number of generators online and whether the switchboards are operating in a split or closed bus configuration. The latter is dependent on the level of resilience required during a particular operating state. The fourth element is the climate condition, the three climate conditions applied to the warship in this work are temperate, tropical and winter, the difference between the conditions relates to the hotel load requirement, which is proportional to the Heating Ventilation and Cooling (HVAC) load. Temperate conditions assume seawater temperature of 18°C with air temperature of 25°C. Winter conditions assume minimum seawater temperature of 0°C, with air temperature of -10°C. Tropical conditions assume a maximum seawater temperature of 30°C and air temperature of 40°C.
Two operating profiles were identified for the candidate frigate, as shown in Fig. 3-2. The first operating profile is a typical Anti-Submarine Warfare (ASW) profile, derived from Partridge and Thorp, (2014) where a significant proportion of operating time is at low speed in the patrol operating state. The second operating profile is general-purpose (GP), derived from Newman and Simmonds (2018). For both profiles the ship is assumed to be operating at sea for 5,000 hours (Newman and Simmonds, 2018).

![Fig. 3-2. Candidate ship operating profiles](image)

The power versus speed curve in Fig. 3-3 for the ship was calculated by extrapolating the cubic relationship based on the shaft power requirement as defined in Table 3-1 for 12 kts economic speed. The power-speed curve was not supplemented with a sea margin or design margin. These were assumed to have been included in the reference data from Gemmell et al. (2014). The percentage operating time is also shown for each profile on the secondary y-axis.

![Fig. 3-3. Candidate ship shaft power versus ship speed curve](image)

The baseline temperate hotel load is 2.5 MW (Gemmell et al. 2014), increasing to 2.75 and 3 MW for tropical and winter climate conditions respectively. The total electrical power generation required in each operating state for each climate condition is shown in Fig. 3-4, which includes the hotel load and the demand from the electric propulsion motors. The propulsion demand includes losses from the propulsion motors to
the generators assumed as those in Table 3-3. It is acknowledged that the efficiency of the propulsion converter from Benatmane and Salter (2018) and Newman and Simmonds (2018) is high, and may in practise be lower. The power and propulsion system operating configurations during the operating states are described in the following sub-sections, these were confirmed as appropriate following discussions with BMT Defence Services and naval ship operators.

![Operating state electrical power generation requirement with climate condition](image)

**Fig. 3-4. Operating state electrical power generation requirement with climate condition**

<table>
<thead>
<tr>
<th>System component</th>
<th>Efficiency</th>
<th>Reference</th>
</tr>
</thead>
<tbody>
<tr>
<td>Switchboard</td>
<td>99.8%</td>
<td>(Benatmane and Salter, 2018)</td>
</tr>
<tr>
<td>Low voltage propulsion converter</td>
<td>98.0%</td>
<td>(Benatmane and Salter, 2018; Newman and Simmonds, 2018)</td>
</tr>
<tr>
<td>Propulsion motor</td>
<td>96.0%</td>
<td>(Benatmane and Salter, 2018; Newman and Simmonds, 2018)</td>
</tr>
<tr>
<td>Shaft</td>
<td>98.0%</td>
<td>(Gemmell et al., 2014)</td>
</tr>
<tr>
<td>Power transmission</td>
<td>99.0%</td>
<td>(Gemmell et al., 2014)</td>
</tr>
</tbody>
</table>

### 3.2.2 Manoeuvring

When the ship is operating during low speed manoeuvring (<5 kts), electric drive would be the preferred mode of propulsion. The preferred power system configuration would be all four DGs running in a split island configuration for redundancy as shown in Fig. 3-5. Although the DGs would be operating at low load, power availability to ship services and propulsion is assured to meet the demands of the prevailing situation. The ESS in this configuration would act in the load levelling mode and/or power reserve mode. During load levelling the ESS would supply the excess load demand whilst the DGs are held at their optimum load condition. When used as a power reserve the ESS could allow a reduction in the number of DGs on line, depending on the capacity of the ESS to act as a power reserve.
3.2.3 Patrol

During patrol operations the ship is envisaged as operating with a minimum of 2 DGs with bus ties closed (Fig. 3-6). The speed range here is anticipated to be between 6 and 9 kts. If installed, the ESS operating modes applicable to the patrol state are the same as for manoeuvring.

3.2.4 Transit

During transit the ship’s power system is to be configured as during patrol (Fig. 3-6), the number of DGs connected to and feeding the switchboard would be as determined by the load. At a transit speed of 12 kts the demand from the propulsion motors is 2.5 MW. The ESS operating modes in the transit state are the same as Patrol and Manoeuvring.
3.2.5 Replenishment-at-Sea (RAS)

The speed during a RAS operation is assumed to be 12 kts (Stevens et al., 2017). The ship will be in electric drive, to provide two independent sources of propulsion for redundancy. It is unlikely that the ship would be propelled by the GT as this is the single point of failure in the propulsion system during RAS. If a failure were to occur the ship is unlikely to be able to break away from the refuelling tanker. For resilience, the power system is likely to be running all four DGs in a split island configuration as in Fig. 3-7. Due to the high resilience required in this operating state, the ESS would most likely operate in the power reserve mode.

![Diagram of RAS power system configuration]

Fig. 3-7. Power system configuration during RAS

3.2.6 High speed cruise

When the ship is transiting at high speed (>16 kts), the GT drive would be enabled. The power system is anticipated to be running two DGs in single island configuration (Fig. 3-8). An ESS here could either operate in the load levelling and/or power reserve modes.

![Diagram of high speed cruise power system configuration]

Fig. 3-8. Power system configuration during high-speed cruise
3.2.7 State 1

When the ship is operating in State 1 (Action Stations), the ship is likely to be in GT drive to provide speed flexibility up to the ship’s top speed. All four DGs would be running and main bus ties open in a split island configuration for redundancy (Fig. 3-9). Split island configuration is common in this operating state to avoid a blackout in case of failure, damage or fault on either of the islands (Southall and Ganti, 2018; Whitelegg et al., 2015). The hotel load in State 1 for temperate conditions is assumed as 3 MW. The ESS operating mode here would be to act as the direct power supply to a LDEW.

![Fig. 3-9. Power system configuration during State 1](image)

3.2.8 Operating profile, state and configuration summary

A summary of the operating profile power requirements, power system operating configuration and ESS functions is provided in Table 3-4.

<table>
<thead>
<tr>
<th>Operating state</th>
<th>Ship speed (kts)</th>
<th>ASW time (%)</th>
<th>GP time (%)</th>
<th>Prop demand (MW)</th>
<th>Winter/ topical/ temperate hotel load (MW)</th>
<th>Prop mode</th>
<th>Baseline system configuration</th>
<th>ESS operating mode</th>
</tr>
</thead>
<tbody>
<tr>
<td>Manoeuvring</td>
<td>≤ 5</td>
<td>6</td>
<td>8</td>
<td>0.18</td>
<td>3.0/2.75/2.5</td>
<td>Electric</td>
<td>Split bus, 4 x DGs</td>
<td>Power reserve</td>
</tr>
<tr>
<td>Patrol</td>
<td>8</td>
<td>45</td>
<td>23</td>
<td>0.75</td>
<td>3.0/2.75/2.5</td>
<td>Closed bus, 2-3 x DGs</td>
<td>Load levelling and power reserve</td>
<td></td>
</tr>
<tr>
<td>Transit</td>
<td>12</td>
<td>30</td>
<td>50</td>
<td>2.53</td>
<td>3.0/2.75/2.5</td>
<td>Closed bus, 2-3 x DGs</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Replenishment-at-sea (RAS)</td>
<td>12</td>
<td>3</td>
<td>3</td>
<td>2.53</td>
<td>3.0/2.75/2.5</td>
<td>Split bus, 4 x DGs</td>
<td></td>
<td></td>
</tr>
<tr>
<td>High speed</td>
<td>24</td>
<td>6</td>
<td>6</td>
<td>19.16</td>
<td>3.0/2.75/2.5</td>
<td>Mechanical</td>
<td>Closed bus, 2 x DGs</td>
<td>Load levelling and power reserve</td>
</tr>
<tr>
<td>State 1 (Action stations)</td>
<td>20</td>
<td>10</td>
<td>10</td>
<td>11.09</td>
<td>3.5/3.25/3.0</td>
<td>Split bus, 4 x DGs</td>
<td>LDEW pulse load</td>
<td></td>
</tr>
</tbody>
</table>

Table 3-4: Summary of candidate ship operating states and ESS operating mode in each state
3.3 LDEW concept of operation

The LDEW in a shipboard environment is envisioned to have scalable capability that allows the weapon to fulfil different tasks to act as a deterrent to threats or act to inflict full structural damage (Daw, 2015), this is achieved by tailoring the laser output power to the desired effect against the target for a given scenario. The benefits of shipboard LDEW include fast engagement times, ability to counter rapidly manoeuvring missiles, conduct precision engagements and conduct graduated response to threats (O’Rourke, 2019). The latter ranges from detecting and monitoring, to engaging targets to cause disabling damage, this is dependent upon the optical power of the LDEW. At low power, the LDEW could be used to damage the sensors of UAVs, preventing the UAV from conducting its role. High powered lasers are recognised as being short-range defensive weapons to inflict structural damage, with a range expectation of up to a few miles (O’Rourke, 2019). An LDEW in a shipboard power system will draw energy ultimately from the fuel used to generate the onboard power via the prime movers. Therefore, an added benefit of drawing the power to supply the LDEW from the power system is the relative cost per shot to conventional chemically propelled weapons stored in magazines (Mills et al., 2018; O’Rourke, 2019).

Efforts to develop laser systems for naval application have concentrated on three types of electrically powered lasers; slab solid-state, fibre solid-state, and free electron lasers (Moran, 2012; Petersen et al., 2011). The SSL are of interest for medium power lasers (<600 kW optical power (O’Rourke, 2015)), and free electron lasers for high power in the multi-MW range (Moran, 2012). However, as discussed in chapter 2, free electron lasers are still in the early stages of development and thus the research focus here is centred around SSLs.

Detailed descriptions of laser technologies are offered by McAulay (2011) and Titterton (2015) for military applications. Titterton (2015) specifies that lasers are characterised by the gain medium, (solid, liquid or gas) and the pumping process (either light energy, electricity or chemical reaction) used to create population inversion. Laser action is dependent on transition of the energy states of the laser specie (ions, atoms or molecules) in the gain medium. Population inversion is the reversal of normal population of the energy states in the laser specie of the gain medium, and necessary for stimulated emission to facilitate laser action (i.e. generation of photons in the laser). In the laser, mirrors are arranged to enable photons to pass back and forth, as the number of passes increases, the optical power of the laser increases, a partially transparent surface allows a fraction of the photon flux to be emitted in the form of a laser beam. The above process is summarised by the simple schematic in Fig. 3-10 (Titterton, 2015).

![Gain medium](https://example.com/gain_medium.png)

**Fig. 3-10. Premise of laser (Titterton, 2015)**
In slab SSLs, the solid gain medium is typically a crystalline material doped with elements with appropriate structures to allow laser light generation by stimulated emission. Slab SSLs implement semiconductor diode lasers to perform the pumping action and use current flow through the junction of the diode to excite the solid-state gain medium to convert the electricity to facilitate laser action.

The equivalent circuit of a laser diode can be represented by a parallel RLC circuit as described by Katz et al. (1981) and shown in Fig. 3-11 with representative values. The parameters of the equivalent circuit in Fig. 3-11 describe the laser diode differential resistance, $R$, the stimulation radiation induced inductance, $L$ (light output from the laser diode) and the diffusion capacitance of the diode, $C$. Diffusion capacitance is the change in charge due to excess carriers in the diode with respect to the voltage of the diode. The equivalent circuit of the laser diode is dominated by the diode differential resistance (Katz et al., 1981).

![RLC Circuit](image)

**Table 3-5**

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Indicative value</th>
</tr>
</thead>
<tbody>
<tr>
<td>$R$</td>
<td>$\sim 1 \Omega$</td>
</tr>
<tr>
<td>$C$</td>
<td>$3 \text{ nF}$</td>
</tr>
<tr>
<td>$L$</td>
<td>$\sim \text{ pH}$</td>
</tr>
</tbody>
</table>

Fig. 3-11. Laser diode equivalent circuit and indicative parameters from Katz et al. (1981)

In high-powered SSLs such as those for shipboard systems, hundreds of laser diodes are combined to form an array (McAulay, 2011; Moran, 2012). In fibre SSLs, such as the LaWS demonstrator discussed in chapter 2, the pumping process of the semiconductor diodes is via a solid fibre medium. The 33 kW LaWS prototype combined six 5.5 kW SSL arrays to form a single beam.

The profile of the LDEW load is important for this research, as it will influence the dynamics of the power system performance. For both technologies the dwell time period (time spent on a specific target for a recognisable length of time) for most laser targets is in the order of seconds (McAulay, 2011; Moran, 2012). SSLs also have a delay characteristic known as time to full radiant intensity. This delay, typically in the tens of milliseconds, is the time delay to achieve full radiant intensity at the laser aperture (Titterton, 2015).

As discussed in Chapter 2, LDEW power requirements for SSLs are expected to reach 2 MW (Mills et al., 2018), which is commensurate with the 600 kW optical power of slab SSLs predicted by O’Rourke (2015) assuming 30% efficiency of the laser. To align with literature, the characteristics for the LDEW in this work are based on the power requirements defined by Mills et al. (2018) and O’Rourke (2015) for the power requirement and four minute engagement time requirement that is the design intent for future warships as defined by Markle (2018), summarised in Table 3-5. While Titterton (2015) did not specify the exact time to full radiant intensity for solid state laser technology, the sensitivity of this parameter on system performance will be explored in more depth in chapter 6.
Having described the candidate ship power system, operating profile and LDEW concept of operation, it is now necessary to define the ESS operating mode requirements that a single ESS needs to fulfil for the ship’s power and propulsion system. Understanding of the operating mode requirements will influence the decisions of the system designer when considering how the ESS needs to interact with the power system and how this influences where the ESS should be electrically integrated within the power system architecture. The ESS operating mode requirements for this research are detailed in Table 3-6.

<table>
<thead>
<tr>
<th>Operating mode</th>
<th>Requirement</th>
<th>Priority</th>
</tr>
</thead>
<tbody>
<tr>
<td>1. LDEW power supply</td>
<td>To provide power to directed energy laser weapons characterised by high power short duration repetitive operation representative of the expected profile defined in Table 3-5.</td>
<td>Primary</td>
</tr>
<tr>
<td>2. Power reserve</td>
<td>To act as an electrochemical power reserve for the ships diesel generator sources to increase the generator loading margins, and where possible enable reduced or single generator operation. The period for power reserve in this work was set to three minutes to allow 3 consecutive start attempts of a DG should a failure occur.</td>
<td>Secondary</td>
</tr>
<tr>
<td>3. Load levelling</td>
<td>To enable dynamic load support through load levelling the DGs by absorbing or supplying energy for deficits in power system demand.</td>
<td>Secondary</td>
</tr>
</tbody>
</table>

Each operating mode requirement in Table 3-6 could yield an ESS solution with differing characteristics in different power system locations, however in this instance, the most demanding mode is operating the LDEW pulse load to support combat system capability. Thus integrating the ESS to support the pulsed load takes precedent over the power reserve and load levelling operating modes when considering the siting and sizing of the ESS, therefore the LDEW is allocated as the primary requirement, load levelling and power reserve are denoted as secondary.
3.4.1 Candidate ESS

The integration of a battery ESS is not limited to an electrical performance perspective, there are practical and physical limitations in the context of the wider candidate warship design that should be noted by the naval design authority.

Practical implications of installing the battery ESS in a warship

The battery technology must be compatible with the naval shipboard environment and the associated constraints imposed by naval architecture considerations. Important naval architecture considerations include, but are not limited to, space, weight, stability and payload. The battery system, like other components of the power system, must physically fit within the volume and weight limits that constrain naval ship design, therefore the power and energy density of the candidate battery ESS is important.

Battery systems introduce potential hazards to the shipboard environment that need to be mitigated in the ship design stage. Potential hazards include arcing, electrocution, fire, cell overpressure, leakage and venting of flammable gases (Watts et al., 2017b). These hazards impose constraints on where the battery ESS is located in the ship general arrangement. Consequently, the battery ESS is likely to be located in a dedicated compartment that is sufficiently insulated to the same level as a Category A machinery space, to protect personnel and other spaces in the ship, should a fire for example, occur from the battery system. The dedicated compartment would likely include a fixed fire suppression system, atmosphere control equipment and have sufficient separation from other spaces that comprise power generation equipment, the latter factor will contribute to the survivability of the power system.

The proximity of the ESS to the LDEW is also important, it is desirable to minimise the distance of the cables between the equipment to minimise the cable impedance. A larger impedance could also limit the ability of the ESS to meet the time to full radiant intensity requirements of the LDEW, and potentially could require oversizing the power rating of the ESS. Therefore, the battery ESS could require location on decks higher than conventional machinery spaces. Further practical consideration needs to be given to physically protecting battery module and cell from penetration during shock events. Penetration from a projectile could introduce a short circuit event in the battery, causing the cell to go into thermal runway and release flammable, toxic and corrosive gases (Alnes et al., 2017). Therefore the mechanical and volumetric properties of the ESS cabinet, and design of the ventilation system in the battery space are practical issues that need to be considered during detailed design of the ESS and warship. Atmosphere control with a dedicated vent system to atmosphere could be required to manage the venting of gases from the cells during a thermal runway event. Such atmosphere control equipment would impose additional space and weight requirements on the candidate warship.

The factors influencing the selection, installation and operation of battery ESS stated above would all form part of the risk analysis for the safety case to integrate a Li-ion based battery ESS with the candidate warship. This thesis primarily considers the battery ESS selection and electrical integration perspective.
Battery electrochemistry and candidate technology

With regard to selecting the chemistry of the candidate battery ESS, as concluded in chapter 2, there have been a number of publications that have investigated LFP based battery systems for warship application in the literature, however NMC has yet to be investigated despite its balanced energy/power characteristic, power and energy density, and competitive thermal runway temperature compared to alternative chemistries. It was further concluded in chapter 2 that due to investment in cell chemistry in the automotive sector, there is continued development of nickel rich batteries (Kim et al., 2019), consequently NMC is used extensively in the automotive and commercial marine sectors. However, there has been a lack of investigation for warship power system applications, therefore to advance knowledge in this field, NMC is the selected cell technology for investigation in this research. Thermal runway temperature of the cells in the battery system was identified as an important characteristic for naval application and influenced the selection of NMC in this research, as NMC has a favourable runway temperature to other potential cell technologies, with the exception of LFP cells.

The battery cell technology used in this research is based upon a 4.2 V, 64 Ah Li-ion polymer pouch type cell with a lithium nickel manganese cobalt cathode and graphite anode. Twenty-four cells combine to form modules, twenty-two series connected modules form a string as described by Table 3-7 (confirmation letter from Corvus Energy in Appendix A). The battery string specification in Table 3-7 is representative of the Orca system from Corvus Energy, each string rated at 125 kWh. The reasons for selecting this specific technology are twofold. First, the technology is proven for application in the marine environment for commercial applications including offshore DP vessels. This is important because even though DP vessels differ in application to those considered in this research, it is an application that requires controlled intermittent bursts of high power for station keeping during operations. Second, as will be described in chapter 4, the battery model developed in this research has been validated against the Corvus Energy Orca system used in marine applications, increasing the relevance and usefulness of this research.

Table 3-7: Candidate battery ESS parameters (confirmation letter from Corvus Energy in Appendix A)

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Module</th>
<th>String</th>
<th>Unit</th>
</tr>
</thead>
<tbody>
<tr>
<td>Nominal capacity (1C rate)</td>
<td>128</td>
<td>128</td>
<td>Ah</td>
</tr>
<tr>
<td>Peak voltage</td>
<td>50</td>
<td>1,100</td>
<td>V</td>
</tr>
<tr>
<td>Nominal voltage</td>
<td>45</td>
<td>980</td>
<td>V</td>
</tr>
<tr>
<td>Cut-off voltage</td>
<td>36</td>
<td>800</td>
<td>V</td>
</tr>
<tr>
<td>Peak cont. discharge current (6C)</td>
<td>768</td>
<td>768</td>
<td>A</td>
</tr>
<tr>
<td>Peak charge current (3C)</td>
<td>384</td>
<td>384</td>
<td>A</td>
</tr>
<tr>
<td>Cells</td>
<td>24 (12s2p)</td>
<td>528</td>
<td></td>
</tr>
<tr>
<td>Size</td>
<td>1.36</td>
<td></td>
<td>m³</td>
</tr>
<tr>
<td>Mass</td>
<td>1,550</td>
<td></td>
<td>kg</td>
</tr>
</tbody>
</table>

3.4.2 Integration options

Fig. 3-12 illustrates two potential locations for the energy storage within the candidate power system. These are commensurate with potential locations for warships as suggested by Hodge and Mattick (1995),
Bellamy and Bray (2015) and Radan et al. (2016). Due to the large power requirements of anticipated pulsed loads, consideration for the integration of ESS on a zonal subsidiary low voltage switchboard is not appropriate due to the large currents that would be drawn by the LDEW. The integration interfaces at locations A and B identified in Fig. 3-12 are first presented and then contrasted in greater detail in the following sub-sections to assess how the location of the ESS impacts power system performance under LDEW loading and therefore make an informed recommendation as to where to locate the ESS for further investigation in this research.

**Fig. 3-12. ESS integration options with the candidate warship power system**

Option A and B are presented in more detail in Fig. 3-13 for a half ship set and discussed in the following subsections. For each option the LDEW pulse load is either located on the main distribution bus (option A1 and B1) or local to the energy store (A2 and B2).
3.4.3 Option A

Integrating the ESS with the main switchboard requires a bi-directional DC-AC converter to charge and discharge the battery, as determined in chapter 2, this is most commonly facilitated via fully controlled pulse width modulated (PWM) power converters (R. D. Geertsma et al., 2017). Discussion on how these converters are controlled to permit bi-directional power flow is provided in chapter 4. The circuit diagram for Option A1 is commensurate with the ESS integration circuits proposed by Radan et al. (2016) and Southall and Ganti, (2018) and Barrera-Cardenas et al. (2019). The passive components constitute a DC and AC side filter, associated with power quality (both drawn from the battery and supplied to the ship) with the latter being influential in the control of the converter. The AC side filter is ungrounded to prevent the circulation of common mode currents between the AC and DC side filter. Integrated with each switchboard of the baseline electrical system is a LDEW with associated rectifier power converter.

Switching behaviour of the power electronic devices is usually the most significant source of common mode voltages within a power system (Doerry and Amy, 2018). The high gradient edges of the high frequency PWM can cause oscillatory effects which lead to over-voltages and EMC issues in common and differential mode (Southall and Ganti, 2018). Use of an input delta-wye transformer is a robust method of providing galvanic isolation and preventing common mode currents from propagating from the bi-directional converter to the distribution system. This is because the delta set of windings do not have a neutral point, and common mode voltage is the difference between the neutral point and the system reference ground voltage (in this case the hull), therefore, the common mode current is forced to circulate in the delta windings of the transformer (Doerry and Amy, 2018; Wakileh, 2001a). Inclusion of the transformer also allows the secondary side voltage to be selected to match the battery string voltage window.
Problem formulation

(Barrera-Cardenas et al., 2019; Radan et al., 2016). This can be shown mathematically for the condition when the battery is discharging to the grid. Consider that the sinusoidal PWM converter relationship between the grid phase voltage and DC link voltage of the DC/AC converter is given by equation (3.1).

\[
\hat{v}_{ab1} = m_a V_{DC} \frac{\sqrt{3}}{2}
\]  

(3.1)

where \( \hat{v}_{ab1} \) is the amplitude of the phase voltage, \( V_{DC} \) is the DC link voltage and \( m_a \) is the amplitude modulation index (Espinoza, 2007). The battery string voltage window is between 1,100 V and 800 V (Table 3-7). Thus, to achieve the rated voltage of the main bus, \( \hat{v}_{ab1} = 976 \text{ V} \) (690 \( V_{max} \)), \( m_a \) is a minimum of 1.02, increasing to 1.3 at the minimum battery string terminal voltage, both of which are outside the linear modulation region. It is desirable to maintain the modulation index between 0.8 and 0.95 of the converter (Kim et al., 2014), to achieve this the battery voltage would need to be between 1,186 and 1,408 \( V_{DC} \), which exceeds the maximum limit of the battery string (Corvus Energy, 2018).

Adopting third harmonic injection in the PWM control can increase the linear modulation region to become \( 0 < m_a < 1.15 \) (Espinoza, 2007), allowing the battery to operate down to a string terminal voltage of 980V, the battery nominal voltage, however this modulation region is insufficient to cover the entire voltage range of the battery. Therefore, despite the reduction in power density of the system, for the purpose of this comparison between integrating the ESS on the main switchboard or propulsion converter, a transformer is included in Options A1 and A2, and a DC/DC converter in Options B1 and B2. The inclusion of the DC/DC converter is due to the complexity of maintaining DC link voltage, controlling charge and discharge, maintaining linearity in the converter control loops and avoiding injection of low order harmonics at low SOC. The harmonic distortion introduced to the power system could cause thermal stress to electrical machines, insulation stress, equipment damage and disruption to other consumers (Wakileh, 2001b).

In the literature, pulsed load converters interfaced with the main switchboard of AC distribution systems have comprised a six pulse diode rectifier (Scuiller, 2012, 2011) twelve pulse diode rectifier with phase shifting transformer (Kim et al., 2014) or six pulse thyristor rectifiers (Whitelegg et al., 2015). Diode and thyristor rectifiers have poor harmonic performance but are simple and robust devices with high power ratings making them suitable for an LDEW interface. For the purpose of this comparison a six pulse diode rectifier is assumed as the LDEW interface. A drawback of integrating the pulsed load on the distribution bus is that this exposes the propulsion and ship service consumers to transient and harmonic impacts of the pulse load as discussed in chapter 2. To mitigate this, alternative configurations of harmonic filters could be used in options A1 and B1, such as an active filter on the distribution bus. An active filter for example may be more optimal from a power quality perspective, but at a cost to the warship in terms of volume.

Each integration option has an energy transfer equation with corresponding loss characteristic for the discharge phase from the battery to the ship’s loads, and during the charge phase from the ship’s generators to the battery. The steady state energy transfer equation for the discharge process from the battery to the LDEW and subsequent recharge process from the generator to the battery are given in (3.2) and (3.3) respectively.
Problem formulation

\[ Q_{\text{batt}} - \text{loss} \ (\text{DC Link filter} + \text{conduction} + \text{switching} + \text{LCL filter} + \text{tx core} \]
\[ + \text{tx copper} + \text{transmission}) \rightarrow Q_{\text{LDEW}} - \text{loss}(\text{LDEW}) \]  

(3.2)

\[ \dot{m}_{\text{fuel}} - \frac{1}{2}J\omega^2 - \text{loss}(\text{gen} + \text{transmission} + \text{tx core} + \text{tx copper} + \text{LCL filter} \]
\[ + \text{conduction} + \text{switching} + \text{DC Link filter} + \text{batt}) \]  

(3.3)

where \( Q_{\text{batt}} \) is the stored charge in the battery, \( Q_{\text{LDEW}} \) is the input energy to the LDEW, \( \dot{m}_{\text{fuel}} \) is the DG fuel mass flow rate, \( \frac{1}{2}J\omega^2 \) is the inertial energy of the diesel engine. Tx represents the transformer.

3.4.4 Option A2

The key difference between Option A1 and A2 is that the LDEW is co-located with the ESS via a DC/DC converter. The interface for the secondary operating modes remains the same. During LDEW operation in Option A2, the ESS discharges to the LDEW via a DC/DC converter. The latter controls the discharge of the battery to meet the LDEW demand. This has advantages when compared with Option A1 as the energy transfer process is reduced as shown by (3.4), therefore the battery capacity and associated integration interface equipment with the power system can be de-rated compared to Option A1. Moreover, the system inductance from the ESS to the pulse load is reduced, this compliments the rise time of the LDEW pulse to full radiant intensity of the laser.

\[ Q_{\text{batt}} - \text{loss} \ (\text{DC filter} + \text{conduction} + \text{switching} + \text{transmission}) \]
\[ \rightarrow Q_{\text{LDEW}} - \text{loss}(\text{LDEW}) \]  

(3.4)

This option provides further benefits over Option A1 as the ESS and LDEW pulsed load can be isolated from the power system during operation. This mitigates the power distribution system exposure to the LDEW transients, power system stability and QPS concerns under pulse loading. If required the DG could support the battery in supplying the LDEW load, albeit with increased losses over Option A1.

3.4.5 Option B1

In Option B1, the ESS is integrated with the propulsion converter DC link via a DC/DC converter. Utilising the propulsion converter DC link could remove the need for additional converters and associated auxiliaries, thus reducing the integration footprint, which is desirable for warships with volumetric constraints (Benatmane and Salter, 2018; Kim et al., 2014). Although this has been argued as being achievable without a DC/DC converter in order to reduce cost, weight and volume (S. Y. Kim et al., 2015), this poses a challenge to control the charge and discharge of the battery whilst maintaining a stable DC link voltage at the propulsion converter (Radan et al., 2016). This could be exacerbated when a current pulse with high
di/dt induces battery voltage drop across the battery internal resistance, such as an LDEW load. As internal resistance increases with cyclic and calendar aging, the voltage drop across the battery will only increase, magnifying the problem.

The benefit of the configurations in Options B1 and B2 are that the AFE of the propulsion converter can be shared, negating the need for an additional bi-directional DC/AC converter and associated equipment. However, the propulsion converter AFE drive will need to be oversized to permit simultaneous charge of the battery and power to the propulsion motor. Furthermore, the key disadvantage of Option B1 is that if the ship is in electric drive mode, power flexibility is restricted when supplying the pulsed load. When the ESS is discharging to the grid in Option B1 it must simultaneously provide propulsive power, therefore, if the GT is unavailable, ship speed would be limited when operating the LDEW pulsed system. If the DGs could contribute to the pulsed load this could be detrimental to engine life and QPS as discussed in chapter 2 (Lowe et al., 2018; Mills et al., 2018), therefore the preferred mode would be to use the ESS as the direct power supply to the LDEW. DG supply to the LDEW would be a reversionary mode if the ESS is unavailable. Limiting propulsive power is unlikely to be an issue for options A1, A2 and B2, as the ESS can provide the entirety of the pulsed load demand (if sufficiently rated), while propulsion and hotel loads are facilitated by the DGs. Thus providing a more robust system with greater flexibility as the combat and propulsion system power supply are partially decoupled. It should be noted that an element of coupling still manifests in QPS. The energy transfer process is similar to Option A1 with the exception of the transformer losses, and is described by (3.5).

\[
Q_{\text{batt}} = \text{loss}(\text{DC Link filter} + \text{conduction} + \text{switching} + \text{LCL filter } + \text{transmission}) \rightarrow Q_{\text{LDEW}} = \text{loss}(\text{LDEW})
\]  

(3.5)

### 3.4.6 Option B2

In Option B2, the LDEW is integrated with the DC link of the propulsion converter via a DC/DC converter. The advantage of this is similar to that of Option A2 regarding the short transmission distance to the pulse load, therefore minimising inductance. However, there is a drawback in controlling the DC link voltage to ensure power is provided simultaneously to the propulsion motor if in electric drive, and the LDEW. The energy transfer equation is the equivalent to (3.4) for Option A2, apart from there being additional losses for the ESS DC/DC converter.

### 3.4.7 Recommended location

A summary of the integration equipment for each option is provided in Table 3-8 and a comparison of the benefits and limitations of each option is provided in Table 3-9.
Table 3-8: Option equipment comparison

<table>
<thead>
<tr>
<th>Option</th>
<th>A1</th>
<th>A2</th>
<th>B1</th>
<th>B2</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>DC link filter</td>
<td>DC link filter</td>
<td>DC/DC ESS converter</td>
<td>DC/DC ESS converter</td>
</tr>
<tr>
<td></td>
<td>DC/AC converter</td>
<td>DC/AC converter</td>
<td>DC/DC LDEW converter</td>
<td>DC/AC diode rectifier</td>
</tr>
<tr>
<td></td>
<td>LCL filter</td>
<td>LCL filter</td>
<td></td>
<td></td>
</tr>
<tr>
<td></td>
<td>Y-Δ Transformer</td>
<td>Y-Δ Transformer</td>
<td></td>
<td></td>
</tr>
<tr>
<td></td>
<td>DC/AC diode rectifier</td>
<td>DC/DC converter</td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

Table 3-9: Summary of ESS and LDEW integration option benefits and limitations

<table>
<thead>
<tr>
<th>Integration option</th>
<th>Benefit</th>
<th>Limitation</th>
</tr>
</thead>
<tbody>
<tr>
<td>A1</td>
<td>• Transformer prevents penetration of common mode currents.</td>
<td>• Diode rectifier exposes system to harmonics and LDEW transients.</td>
</tr>
<tr>
<td></td>
<td></td>
<td>• Additional equipment compared to B1 and B2.</td>
</tr>
<tr>
<td>A2</td>
<td>• Low inductance from ESS to pulse load.</td>
<td>• Additional equipment compared to B1 and B2.</td>
</tr>
<tr>
<td></td>
<td>• Mitigates transients and harmonics from main distribution system under LDEW operation.</td>
<td></td>
</tr>
<tr>
<td></td>
<td>• Decouple fight and move capability.</td>
<td></td>
</tr>
<tr>
<td>B1</td>
<td>• Reduced equipment volume and mass impact to candidate ship compared to A1 and A2.</td>
<td>• Power supply flexibility reduced.</td>
</tr>
<tr>
<td></td>
<td></td>
<td>• Propagation of common mode currents in absence of isolation transformer.</td>
</tr>
<tr>
<td></td>
<td></td>
<td>• Complex control whilst maintaining QPS for propulsion.</td>
</tr>
<tr>
<td>B2</td>
<td>• Reduced equipment volume and mass impact to candidate ship compared to A1 and A2.</td>
<td>• Propagation of common mode currents in absence of isolation transformer.</td>
</tr>
<tr>
<td></td>
<td>• Low inductance from ESS to pulse load.</td>
<td>• Complex control whilst maintaining QPS for propulsion.</td>
</tr>
</tbody>
</table>

By comparing the options in Table 3-9 it has been shown that the benefits of Option A2 outweigh the limitations. Importantly Option A2 has the ability to decouple the transients associated with LDEW operation with ship’s services and the ability to propel the ship using electric propulsion, therefore Option A2 was selected to be investigated in this research, and is described by Fig. 3-14. This circuit integration option is commensurate with Gattozzi et al. (2015) for LDEW operation, and Radan et al. (2016) and Southall and Ganti (2018) for the secondary modes of the battery ESS.
3.5 The impact of operating mode requirements on the battery ESS

Having proffered an explanation of the candidate battery ESS and selected the power system interface, this section provides conceptual understanding of the cell characteristics that impact the ability of a battery ESS to facilitate the ESS operating modes under investigation in this research. This section then takes an analytical approach to explore how the operating mode requirements influence the specification of the battery ESS rating from a power perspective at system level.

3.5.1 Description of battery cell characteristics

While Linden and Reddy (2010) and Root (2011) provide an in depth explanation of the principles of cell electrochemistry, design and operation, this subsection offers appropriate reading and explanation as to how the processes at the cell level will impact the ability of the battery ESS to meet the operating mode requirements stated in Table 3-6.

As discussed in chapter 2, when a load connected across the battery terminals draws current, electrons flow from the anode through the external load to the cathode. Current flow is reversed during the charge state, and the electrodes reverse their roles. The medium for transporting charge is the electrolyte of the cell. There are three key loss mechanisms that occur when current passes through the electrodes of a cell that cause waste heat generation during operation of the cell. These are activation, concentration and ohmic polarisation. Activation polarisation drives the electrochemical reaction at the electrode surface, while concentration polarisation losses occur due to differences in the chemical reactants and products at the cell surface. Ohmic polarisation leads to voltage drop during operation, which is due to the internal impedance of the cell. The total internal impedance is the sum of the ionic resistance of the electrolyte, electronic resistance of the active mass, current collectors and tabs of the electrodes in the cell (Linden and Reddy, 2010a). When connected to an external load the voltage of the cell is expressed as (3.6).
\[ V_{cell} = u_{OCV} - \left[ (\eta_{ap})_a + (\eta_{cp})_a \right] - \left[ (\eta_{ap})_c + (\eta_{cp})_c \right] - I_{load}R_{cell} = IR \]  

(3.6)

where \( u_{OCV} \) is the open circuit (no load) voltage of the cell, \( \eta_{ap} \) and \( \eta_{cp} \) are the activation and concentration polarisation at the anode and cathode, these being donated by subscripts \( a \) and \( c \) respectively. \( I_{load} \) and \( R_{cell} \) are the load current and cell internal resistance respectively. Only at low currents does the cell operate close to its open circuit voltage (OCV) to deliver the approximate theoretically available energy stored by the cell. Therefore, as the cell discharges, the voltage is lower than the theoretical potential of the cell due to IR losses and polarisation of the active material during discharge. The cell will continue to discharge under load until the cut-off voltage is reached. This is defined by Linden and Reddy (2010b) as the voltage above which the most of the battery’s maximum capacity has been delivered. As presented in Fig. 3-15, the shape of the voltage curve varies with the depth of discharge of the cell and operating current. As higher current is drawn, ohmic losses and polarisation effects increase, therefore the discharge voltage decreases with a more prominent \( dv/dt \), as demonstrated by comparing the low C rate voltage curve with up to the 6C rate curve in Fig. 3-15. The 10% to 80% DoD limits that are typically applied during ESS operation (Hannan et al., 2018) have been highlighted in Fig. 3-15 to show the typical working cell voltage window.

![Fig. 3-15. Effect of discharge load current on battery voltage for an NMC cell](image)

To further understand the relationship between the cell and system behaviour, it is important to consider the effects of change in load, and more significantly, under pulsed discharge, the effects of which can be considered for a typical pulsed discharge as shown in Fig. 3-16. The characteristics of the pulse response are dependent on the battery chemistry, cell design, SoC and internal resistance at the time of the current pulse. In Fig. 3-16 current is drawn from the cell at point 1 at 0.5 C rate, at which point there is an instantaneous voltage drop caused by the internal resistance until point 2. Between points 3 and 4 the activation and concentration polarisation effects increase, further reducing the voltage at the cell terminals. The cell is unloaded at point 4, the period succeeding this is known as the voltage relaxation period as the voltage recovers to the OCV value. The pulse discharge characteristics of the candidate ESS are discussed in more detail in chapters 4 and 6.
3.5.2 LDEW power supply

In initially assessing the number of battery strings required for LDEW operation it is first important to understand the steady state power limit of the battery ESS. At the battery terminals the steady state power limit is a function of the open circuit voltage, maximum cell discharge current and the losses due to internal resistance in the cell. At cell level this can be calculated using the following relationship:

\[ P_{\text{cell,lim}} = u_{\text{OCV}} I_{\text{max}} - R_{\text{cell}} I_{\text{max}}^2 \]  \hspace{1cm} (3.7)

where \( P_{\text{cell,lim}} \) is the cell power limit, \( I_{\text{max}} \) is the maximum allowable current drawn from the battery by the load and \( R_{\text{cell}} \) is the internal resistance of the cell. As shown by equation (3.7), cell power will reduce with increasing depth of discharge due to the maximum current limit of the battery and open circuit voltage. Consequently, the steady state power limit of a battery ESS can be formulated as follows:

\[ P_{\text{ESS,lim}} = P_{\text{cell}} n_s n_p n_{\text{mod}} n_{\text{strings}} \]  \hspace{1cm} (3.8)

where \( P_{\text{ESS,lim}} \) is the battery ESS power limit, \( n_s \) and \( n_p \) are the number of series and parallel connected cells in a module, \( n_{\text{mod}} \) is the number of series connected modules in a string for a given number of strings, \( n_{\text{strings}} \). Plotting the steady state power limit of a string as a function of state of charge is a good starting point to identify how many battery strings are required in the ESS, in order to supply the LDEW peak pulse power requirement. This plot must include an estimate of the DC/DC converter losses between the ESS and LDEW. Once the minimum required number of strings in the ESS has been defined, dynamic pulsed discharge analysis can then be conducted to establish whether the number of strings in the ESS is sufficient to supply the LDEW load. The steady state power limit is considered here, before in depth investigation in chapters 4 and 6. The estimate of LDEW power supplied by the ESS is calculated using (3.9).
Problem formulation

\[ P_{\text{ESS.LDEW}} = \frac{P_{\text{LDEW}}}{\eta_{\text{transmission}}\eta_{\text{DC/DC}}} \]  

(3.9)

where \( P_{\text{LDEW}} \) is the laser power, and \( \eta_{\text{transmission}} \) and \( \eta_{\text{DC/DC}} \) are the transmission and DC/DC converter efficiency respectively. For the purpose of this discussion, the LDEW load profile can be characterised by a pulse on time, pulse off time, and fast rise time/fall time as shown in Fig. 3-17 (Kim et al., 2014; Scuiller, 2012). Studies have approximated the pulse load as a pure square wave (Allan and Jones, 2015; Lowe et al., 2018), however the rise time of the pulse in reality will be limited by the time to full radiant intensity of the laser, converter time constant and input inductor rate of rise of current limit. Importantly \( T_r \) and \( T_f \) are extraordinarily short and this has corresponding impacts on the battery response.

![LDEW trapezoidal pulse load profile](image)

Fig. 3-17. LDEW trapezoidal pulse load profile

From a dynamic perspective the fast rise rate, high current characteristic of an LDEW load will result in an ohmic transient voltage drop during current excitation of the cells in the battery ESS, as previously described. The importance of this characteristic is that due to the high current, and depending on the state of charge, battery terminal voltage drop could be sufficient to surpass the cut-off voltage, therefore triggering the battery protection system to prevent the battery from continuing to discharge to the operational LDEW (Confirmed by Sean Yamana via email from Corvus Energy, 21/06/2019). The extent of the voltage drop due to the LDEW characteristic could limit the useable SoC range of the battery under LDEW operation. Further this is influential to the converter dynamics and control system. This will be discussed in more detail in chapter 4.

3.5.3 Power reserve

The following analysis demonstrates the impact of the battery ESS power rating on the generator loading margins. Understanding this impact could potentially improve fuel consumption, cognisant of the resilience required when operating a warship power system. The method used to determine the generator load limit during each operating state with and without the ESS has been modified from Kim et al. (2015), Radan et al. (2016) and Southall and Ganti (2018) to include the estimated losses between the ESS and the switchboard. The power generation limit when ESS is integrated with the power system is defined as (3.10),
where:

- $P_{G,\text{limit}}$ is the online generator load limit
- $\sum P_{G,\text{Nonline}}$ is the total generating capacity of the N generators online and supplying power demand
- $\sum P_{\text{ESS}}$ is the ESS power available at the switchboard, after power conversion and transmission losses at peak discharge rate.
- $\sum P_{G,\text{failed}}$ is the generating capacity lost due to N failed generators, in this work the failure mode assumes N to be 1.

For the purpose of steady state analysis, the assumption is made that the generators are required to share the load equally. In reality, under dynamic operation the generator sets are permitted to deviate from being balanced by up to a maximum up of $\pm25\%$ of the individual machine rating in real power and $\pm5\%$ in reactive power (Lloyd’s Register, 2017). The load limit for each generator set online and supplying load in per unit form can be defined as a ratio of the online load limit to the generating capacity after a generator failure:

$$\beta_{G,\text{limit}} = \frac{P_{G,\text{limit}}}{\sum P_{G,\text{Nonline}}}$$ (3.11)

As such, the three minute power reserve duration requirement permits sizing analysis to be conducted on the battery ESS capacity, cognisant of the desired power:

$$\text{Energy}_{\text{ESS, reserve}} = P_{\text{ESS, reserve}} T_{\text{ESS, reserve}}$$ (3.12)

where $\text{Energy}_{\text{reserve}}$ is the required battery capacity given the required ESS power, $P_{\text{ESS, reserve}}$ for the reserve duration, $T_{\text{ESS, reserve}}$. Depending on the reserve power rating of the ESS at a given SoC, the battery internal resistance and therefore the battery terminal voltage drop needs to be taken into consideration to ensure the battery provides continuity of power without dropping below the cut-off voltage limit.

### 3.5.4 Load levelling

The power and energy delivery duration requirements of the LDEW are the governing drivers of the battery ESS power and capacity rating. Therefore, rather than examine how the load levelling operating mode impacts the ESS sizing, it is pertinent here to state the operating relationships between the battery ESS and DG when operating during load levelling.
During load levelling the power requirement can be deduced from the load balance in (3.13), where the DG is held at constant load and the battery ESS charged and discharged to handle the load fluctuations subject to the battery ESS power limits defined by (3.14), SoC limits (3.15) and DG power limit settings (3.16).

\[ \sum P_L = P_{DG} + P_{ESS} \]  
\[ p_{Max}^{charge} \leq P_{ESS} \leq p_{Max}^{discharge} \]  
\[ SoC_{min} \leq SoC_{ESS} \leq SoC_{max} \]  
\[ p_{Min}^{DG} \leq P_{DG} \leq p_{Max}^{DG} \]

where \( P_{DG}, P_{ESS} \) and \( P_L \) are the DG, ESS and load real power, \( p_{Max}^{charge} \) and \( p_{Max}^{discharge} \) are the maximum charge and discharge power. \( SoC_{min} \) and \( SoC_{max} \) are the SoC limits during operation to prevent degradation of battery capacity, discussed in chapter 2 as being between 20 to 90% SoC.

3.6 Power conversion, control and power management relationships between the ESS and the power system

Thus far, the potential impact of the ESS operating modes have been explained from the perspective of the battery. However, operation from the perspective of the power system needs to be understood. This section will propose suitable power conversion equipment suitable for interfacing the ESS with the LDEW load and power system, before examining the control and power management relationships for each operating mode.

3.6.1 LDEW DC/DC converter characteristics

As previously discussed, isolating the ESS and LDEW from the main power distribution system will mitigate against adverse QPS conditions arising from powering the LDEW with the DGs. This method is commensurate with the approach by Gattozzi et al. (2015) and Whitelegg et al., (2015). Fig. 3-18 shows a simplified diagram of the ESS DC/DC converter interface with the LDEW load. The DC/DC converter is of significant importance as it ensures that a pulse of the desired characteristics is delivered to the load by the battery whilst maintaining QPS on the LDEW bus.
As detailed in chapter 2, there has been limited published information on converter and controller interfaces for LDEW loads, or on the DC voltage level of an LDEW pulse load for the peak 2 MW pulse power investigated here. To enable the formulation of discussion in this research, the LDEW voltage level was selected as 1,500 V, as this is a voltage level recommended by industrial practice for DC ship power systems by IEEE 1709 for 1 kV to 35 kV DC systems (IEEE Industry Applications Society, 2018). Moreover, this is the closest recommended DC voltage to the battery ESS nominal voltage of 980 V. Consequently, the converter selected to control the discharge from the battery to the LDEW is a single stage DC/DC boost converter. While there are other DC/DC topologies available, as described by Kazimierczuk (2016), the single stage unidirectional boost topology, shown in Fig. 3-19, was selected here as the voltage transformation between the battery and LDEW is not large. Isolated DC/DC converters such as a full bridge converter could benefit the system if there was a larger voltage ratio, and further by providing galvanic isolation and reduction in electromagnetic interference between the ESS and LDEW side of the converter. However, isolation converters decrease the power density of the system due to increasing the number of semiconductor devices and the addition of a high frequency transformer.

A detailed explanation of DC/DC boost converter operation is offered by Choi (2013), Kazimierczuk (2016) and Hayes and Goodarzi (2018). A brief overview of the fundamentals is given here followed by the key characteristics that will impact the control of the battery to provide the LDEW load while maintaining QPS.

As shown in Fig. 3-19, the boost converter consists of a controlled Insulated Gate Bipolar Transistor (IGBT), SW, input inductor, $L$, diode, $D_1$ and output filter capacitor, $C$. $V_{bess}$ is the battery voltage, $V_L$ is the voltage across the inductor and $V_o$ is the converter output voltage. DC/DC boost converters have two modes of operation, continuous and discontinuous conduction mode (CCM and DCM), relating to the regularity of input inductor current conduction. Due to the high current characteristic of the load, only CCM needs to be discussed. During operation, the IGBT is switched on and off at a frequency of $f_s = 1/T_s$ where $T_s$ is the
switching period with a controlled conduction time of $DT_s$, $D$ is the duty cycle ratio and key control variable for the circuit, determined using (3.17).

$$D = 1 - \frac{V_L}{V_o} \quad (3.17)$$

Fig. 3-20 shows the concept of operation of the DC/DC converter using an idealised equivalent circuit. During the switch on period, $0 < t < DT_s$ (Fig. 3-20(a)), the inductor current increases with a slope of $V_L/L$ consequently the magnetic energy increases and the switch current is equal to the inductor current, $I_L$. Conversely during the switch off period when $DT_s < t < T_s$ (Fig. 3-20(b)), the inductor current ramps down with a slope of $(V_L - V_o)/L$. At this transition the inductor acts as a current source and turns the diode on. During this time the energy stored in the inductor transfers to the filter capacitor and load. At time $t = T_s$ the IGBT is turned on again to repeat the cycle.

![Diagram](a)

![Diagram](b)

![Diagram](c)

![Diagram](d)

![Diagram](e)

Fig. 3-20. DC/DC boost converter during CCM (a) switch conducting, diode conducting (c) PCC voltage (d) inductor voltage and (e) inductor current

Due to the structure of the circuit, the boost converter is a non-minimum phase system because it has a right-hand plane (RHP) zero in the numerator of the duty ratio-to-output voltage transfer function (Choi, 2013). Therefore, the phase variation is wider for a non-minimum phase system than a regular system. The position of the RHP zero varies with the load resistance, and the input voltage via the duty cycle, as shown by the zero location, that can be determined using (3.18), the derivation for which can be found in Choi (2013). The negative phase as a consequence of the RHP zero gives rise to delay in the system response which could lead to system instability if not managed correctly, which is undesirable under large load disturbances such as the LDEW pulse load examined in this research.
\[
\omega_{\text{RHP}} = \frac{(1-D)^2 R_{\text{pulse}}}{L}
\] (3.18)

There are two possible methods of control for DC/DC boost converters as discussed by Choi (2013), Özdemir and Erdem (2018) and Kasicheyanula and John (2019), these being voltage or current mode control. The former uses only the output voltage signal in a single loop feedback system to control the IGBT duty cycle. However, this method is not appropriate, as the presence of the RHP zero and thus system delay is not alleviated, moreover the inductor current is largely uncontrolled. This is important because the average value of the inductor current is equivalent to the DC current drawn from the battery ESS, which needs to be maintained below the maximum discharge rate. These drawbacks can be somewhat alleviated by current mode control.

Current mode control improves the dynamic performance over voltage mode control because it utilises a double loop feedback system that contains an inner current loop to control the inductor current, therefore battery discharge current, and an outer loop to control the output voltage. In response to a change in inductor current, the inner loop initially adjusts the duty cycle, and the outer voltage loop varies the reference inductor current for the inner loop to respond to any change in output voltage. This is important as the inductor now acts as a voltage dependent current source, therefore to an extent allowing the voltage QPS under pulse loading to be managed at the output of the converter as well as controlling the discharge of the battery ESS. While the double loop principle appears a simple solution, the dynamics are complex and the control problem is exacerbated by the fast rise time and high current characteristic of the LDEW that induces a large transient voltage drop in the battery. The control system principle therefore needs to be adapted to accommodate the wide input voltage variation of the battery, which is dependent on the internal resistance of the battery ESS (as discussed in section 3.5), when facilitating large current drawn by the LDEW load whilst maintaining QPS, this will be explored further in chapter 4.

### 3.6.2 Bi directional DC/AC converter characteristics

For the case of this research a two level, IGBT based, voltage source converter (VSC) locally controls the charge and discharge of the battery to the power system, a schematic for which is shown in Fig. 3-21(b). This converter has been selected as it is a well-documented and recognised method of integrating battery ESS with AC power distribution systems for medium to high power applications. The IGBTs are employed due to their relatively high current capability, coupled with low switching and conduction losses (Radan et al., 2016; Southall and Ganti, 2018; Yazdani and Iravani, 2010). The IGBT has advantages over controlled switches such as Metal Oxide Field Effect Transistors (MOSFETs), IGBTs have a higher current capability and efficiency (Busarello et al., 2018). Contrastingly, Insulated Gate Commutated Thyristors (IGCTs) have reduced on state losses compared to IGBTs, however IGCTs have limited switching frequency up to 500 Hz due to high switching losses at higher frequency, and higher rates of failure (Bosich et al., 2017; Gachovska and Hudgins, 2018). The previous transistors are silicon-based devices, Silicon Carbide (SiC) based devices are of interest for high power applications due to their low switching, low conduction loss and high temperature operation characteristics. However, at present SiC based device adoption in naval
applications is low due to their high manufacturing cost and low availability (Gattozzi et al., 2017). The IGBT was selected for use in this research for both the DC-DC boost converter, and the DC/AC converter, due to their balance of high current capability, efficiency and cost.

The PWM control method is employed to control the switching devices as this is the industry standard for VSC control (Hansen and Wendt, 2015; Yazdani and Iravani, 2010). Whilst three-level neutral point clamped converters provide reduced harmonic distortion compared to two level converters, this is at the cost of an increased number of power electronic devices that reduce the power density of the system. Moreover, the LCL filter at the output of the converter and intrinsic line impedance of the transformer provide additional harmonic attenuation.

![Diagram](image)

*Fig. 3-21. (a) ESS interface during load levelling and power reserve (b) Two-level VSC*

As detailed in chapter 2, the ship power system management during normal operation can be partitioned hierarchically into three layers, primary, secondary and tertiary. In the primary control layer of hierarchical power management systems, power converters can be further classified into three categories: grid-forming, grid-feeding and grid-supporting (Bouzid et al., 2015; Rocabert et al., 2012). The selection of the power converter control is important to the parallel operation of the battery and DG sources during load levelling and power reserve. The latter mode may require the ESS to operate as the sole power source to the grid in the event of generator failure. This in part governs the selection of the power converter control strategy employed in this research.

Grid-forming converters are represented as ideal voltage sources with a given voltage amplitude, $E$ and frequency, $\omega$, these are the sole inputs to the control system as shown in the simplified diagram in Fig. 3-22(a). Grid-feeding converters (Fig. 3-22(b)) are designed to provide power to an already energised power system and are represented as a current source. The converter voltage and frequency is synchronised from the distribution bus, therefore an external reference, such as a generator set, is required to form the grid
voltage and cannot be operated in an islanded mode to solely provide power to the ship. Converse to grid forming, the real and reactive power are controlled in this system. The method employed in this research is grid supporting. In this method the converter is controlled to emulate an AC voltage source. The benefit of this method over the preceding methods is the active and reactive power delivered to the grid can be controlled when paralleled with generator sets. Second, in the absence of a reference from the distribution system, the converter can set its own frequency and voltage set point therefore permitting sole ESS power supply to the ship if required. The grid-supporting converter does not have to communicate with parallel sources in the presence of droop control. The intention of droop control is to avoid circulating currents between power sources, as discussed previously in chapter 2. The inner control system denoted C in Fig. 3-22 usually comprise inner controllers operating in the direct and quadrature axis (Bouzid et al., 2015). The direct component facilitates control of active power and quadrature component, the reactive power. The inner control system is explained fully in chapter 4.

Fig. 3-22. Simplified representation of (a) grid-forming (b) grid-feeding and (c) grid-supporting converter

As previously discussed in chapter 2, the secondary control layer is responsible for eliminating steady state voltage and frequency deviations and are normally centralised due to the size of the ship grid. The tertiary layer is integral to managing the power split between sources, based on a system objective, including whether the battery ESS is operating in the power reserve or load levelling mode. It is recognised that these operating modes could be active simultaneously depending on the SoC of the battery. If the battery ESS is operating in power reserve to provide power to the ship in the event of a generator fault, upon receiving notification of the fault, the tertiary management layer is required to allocate the correct active and reactive power set points to the battery converter primary control layer. This is subject to the limitations of the battery ESS, such as SoC and discharge current. The energy management strategy for the load levelling mode aims to run the DG at its optimum output, while the battery discharges and charges to supply the load transients. Consequently, the DG power set point is held constant and the battery converter set points are changed to match the fluctuating demand, subject to battery operating limitations. Tertiary layer strategies for the load level operating mode are detailed further in chapter 5.

3.7 System performance measures in the context of ESS operation

Thus far, this chapter has suggested a candidate warship hybrid electric power system, for which a candidate ESS and power system interface have been selected and justified. The power management and control relationships have been formulated for the three ESS operating modes of interest in this research.
Table 3-10 describes the research problem of each mode and the performance criteria with which to assess the research problem. Unlike the remaining criteria, there are prescriptive guidelines and recommended practice that pertain to electric power system QPS. These are discussed in the following subsection.

Table 3-10: summary of research problems and performance criteria

<table>
<thead>
<tr>
<th>Operating mode</th>
<th>Research problem</th>
<th>Performance criteria</th>
</tr>
</thead>
<tbody>
<tr>
<td>1. LDEW power supply</td>
<td>• Battery ESS and DC/DC power converter transient response to LDEW pulsed power loads are yet to be fully understood. • The ability of the candidate li-ion based NMC battery ESS to supply an LDEW load to meet a sustained four-minute engagement is unknown. • The extent to which the usable SoC range of the candidate battery ESS reduced is by operating such high current loads with fast rise times is unknown.</td>
<td>• DC bus QPS under LDEW load. • Ability to meet four minute power supply of LDEW load. • SoC range under LDEW loading.</td>
</tr>
<tr>
<td>2. Power reserve</td>
<td>• Warship power system performance with battery ESS acting as power reserve is yet to be analytically quantified for a candidate vessel with battery ESS, cognisant of the ship operating profile, power system configuration and efficiency characteristics of the ESS interface.</td>
<td>• Engine fuel consumption • Engine GHG emissions • Engine running hours • ESS and interface efficiency</td>
</tr>
<tr>
<td>3. Load levelling</td>
<td>• Limited published information on electrical performance and QPS when operating ESS and generators in parallel under load levelling in a warship with a low voltage AC power distribution system.</td>
<td>• Main distribution bus QPS. • Engine fuel consumption • Engine GHG emissions</td>
</tr>
</tbody>
</table>

3.7.1 QPS considerations under LDEW operation, power reserve and load levelling

There are specific standards and recommended practice that govern the QPS of electric power supplies in warships. Whilst the LDEW supply from the battery is considered as isolated from the main distribution system in this research, compliance with QPS standards ensures system integrity when considering wider system integration. Furthermore it ensures the correct operation of equipment that may be connected the DC power supply. IEEE 1709 is a guideline that recommends practice for DC electrical power systems between 1 kV and 35 kV rated voltage and is currently the only set of guidelines applicable to this research. Hence assessing the ability of the battery and converter to maintain QPS against IEEE 1709 recommended practice allows this research to be relevant to the field. Unlike AC power systems, the fundamental frequency of the supply is zero and thus the conventional concept of harmonic distortion does not apply. Instead DC power systems use the premise of voltage tolerance, voltage and current ripple. The recommended voltage tolerances are summarised in Table 3-11, IEEE 1709 does not specify current ripple limits, but stipulates that the ripple value depends on the nature of the power conversion. In this research
this applies to the rate of rise of current to meet the LDEW demand for time to full radiant intensity. This will be discussed further in chapters 4 and 6.

Table 3-11: IEEE 1709 QPS recommended practice summary  
(IEEE Industry Applications Society, 2018)

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Tolerance</th>
</tr>
</thead>
<tbody>
<tr>
<td>Voltage tolerance</td>
<td>±10%</td>
</tr>
<tr>
<td>Voltage ripple (rms)</td>
<td>5%</td>
</tr>
</tbody>
</table>

NATO STANAG 1008 is recognised as the baseline standard for ship AC low voltage electrical power systems are designed to meet (Mills et al., 2018). In the context of this research, this standard applies to the AC main distribution system and governs the constancy of frequency, voltage and voltage harmonic waveform distortion during the ESS load levelling mode. The standard limits are summarised in Table 3-12.

Table 3-12: NATO STANAG 1008 QPS summary (NATO, 2004)

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Steady state tolerance</th>
<th>Transient tolerance</th>
<th>Maximum transient tolerance</th>
</tr>
</thead>
<tbody>
<tr>
<td>Voltage</td>
<td>± 5%</td>
<td>± 16 % (for 2 s)</td>
<td>± 20% (for 2 s)</td>
</tr>
<tr>
<td>Frequency</td>
<td>± 3%</td>
<td>± 4 % (for 2 s)</td>
<td>± 5.5 % (for 2 s)</td>
</tr>
<tr>
<td>Voltage THD</td>
<td>5%</td>
<td>N/A</td>
<td>N/A</td>
</tr>
</tbody>
</table>

3.8 Summary

This chapter has provided a conceptual explanation of the challenges posed when integrating and managing a battery ESS power source within a candidate warship hybrid power and propulsion system. First, the candidate hybrid power and propulsion system, operating profile and power system configurations were detailed. Background reading was provided on lasers for shipboard application and the characteristics of an SSL were selected for investigation in this research. A lithium NMC based battery ESS was selected and the characteristics described. The primary LDEW power supply mode, secondary load levelling and power reserve modes in this research were selected to permit investigation of dynamic, quasi-steady state and steady state performance of the candidate power system respectively.

The integration location and interface design options of the battery ESS were then compared. For the ESS operating modes of interest in this research, it was suggested that a battery ESS should be located on each of the two switchboards in the candidate system, and the LDEW co-located with the ESS via a DC/DC boost converter. This is in order to provide the greater flexibility of the battery ESS, and to decouple the transients of the LDEW load from the main distribution system.

Background reading was then provided on the battery cell characteristics that are pertinent to the battery ESS operating modes. Where it was concluded that the fast rise time and high current of the LDEW could limit the useable SoC range of the battery under such extreme loading conditions due to the internal impedance of the cells. It was suggested that a single stage boost DC/DC converter be employed between the battery ESS and the LDEW load, due to the relative voltage difference between the nominal voltage of the battery and the LDEW output voltage. The DC/DC converter control characteristics were analysed and
Problem formulation

It was highlighted that the current-mode control of DC/DC boost converters requires modification to compensate the voltage drop characteristic of the battery ESS under LDEW loading. Under such conditions it was concluded that whilst the battery ESS, DC/DC converter and LDEW are considered isolated from the power system in this research, QPS standards ensure system integrity and therefore IEEE 1709 should be used to benchmark the impact of LDEW operations on QPS.

Relationships to describe how the battery can increase generator loading margins for power reserve and the power source relationships during load levelling using the battery ESS and DG were described. It was also concluded that during these modes of operation a grid-supporting control strategy should be employed for the VSC that commands the charge and discharge of the battery ESS. This allows the battery to act as a sole power source on each switchboard in the absence of a generator reference for voltage amplitude and frequency. It was decided that since NATO STANAG 1008 is the current baseline standard for warship AC power systems, it therefore should be used as the QPS performance measure under load levelling. Methods to explore the candidate hybrid power and propulsion system performance with battery ESS against the performance criteria stated in section 3.7 will be discussed in the following chapter.
Chapter 4  Modelling

4.1  Introduction

Chapter 3 described the need to understand the dynamic and steady state performance when a Li-ion ESS is integrated with a candidate warship’s electric power system to operate as a power source to LDEWs, act as power reserve and to load level generator sets. Appropriate methods are required to investigate the performance of the power system for each ESS operating mode against the performance criteria as summarised in chapter 3. Whilst small-scale laboratory testing using hardware in the loop provides the advantage of high fidelity, this is often countered by financial cost of the equipment. Conversely, analytical models and time-domain simulations are widely accepted methods that may be used to analyse the performance of battery technology and electric warship power systems over a range of system operations and dynamic events, as described by Bordin and Mo (2019) and Zhu et al. (2017). Analytical and time domain methods are employed in this research.

To advance upon the analysis presented in chapter 3, three modelling tools were developed to investigate the performance of each of the battery ESS operating modes for the candidate warship power system. The candidate power system line diagram is shown in Fig. 4-1 with the battery ESS integrated with the main switchboard and LDEW co-located with the ESS. The first tool that will be detailed is the steady-state modelling tool for the ESS power reserve mode. This is followed by the second tool for time-domain simulation of the load levelling mode under quasi-steady state conditions. The third model is the dynamic time-domain simulation model applied to when the ESS is operating as an LDEW power supply. Following the description of the three modelling tools, descriptions of the constituent models in each tool are offered. These include the model schematic or equivalent circuit, justification for and selection of the model parameters, model assumptions and limitations. This chapter concludes with verification and validation of the constituent models to ensure that the simulations in this research yield credible results.
4.2 Modelling tool 1: Steady state model for power reserve

4.2.1 Modelling purpose

The purpose of modelling tool 1 is to analytically quantify the fuel consumption, exhaust GHG emissions and engine running hour savings as a consequence of introducing the Li-ion battery ESS to the candidate warship power system when operating as a power reserve. As described in chapter 3, the power reserve uses the available energy and output power of the battery ESS to increase the generator loading margins. The extent of any savings is dependent upon the operating profile, power system operating configuration, efficiency characteristics of the ESS interface and fuel consumption characteristic of the prime mover power sources. Hence the first modelling tool purpose is to determine the potential fuel savings the ESS can introduce across the operating profile of the candidate warship electric power system when the ESS is used as a power reserve cognisant of the operating constraints of the power system configuration as described in section 3.2.

The inputs to modelling tool 1 are the ship operating profile, system electrical loads, bus configuration, DG constraints, ESS efficiency map, ESS SoC, ESS capacity and costing data. The outputs from modelling tool 1 are the ship fuel consumption and fuel cost, DG running hours, DG maintenance cost and exhaust GHG emissions per year of operation, based on BoL performance of the battery ESS.

4.2.2 Modelling requirement

The warship electric power system under investigation is as presented in Fig. 3-1 and as described in section 3.2. Justification for the ESS interface with the main switchboards of the power system was also detailed in chapter 3. The extent of modelling tool 1 is limited to the electrical power system performance of the system shown in Fig. 4-2. LDEW loads are not included in modelling tool 1. The electrical loads will be

Fig. 4-1. Candidate warship hybrid power and propulsion system with Li-ion ESS integrated with the main switchboards. The LDEW is co-located with the ESS.
considered to be steady state loads from the perspective of the switchboards. Mechanical propulsion loads are not included in the model. The analytical models comprise the following:

1) Diesel engine fuel consumption
2) Generator efficiency
3) Transformer efficiency
4) VSC efficiency
5) ESS efficiency

![Diagram of components considered in modelling tool 1]

**Fig. 4-2. Components considered in modelling tool 1**

### 4.2.3 Assumptions and limitations

The following assumptions and limitations will apply to modelling tool 1.

**Modelling assumptions**

1) This steady state model assumes that the loads during each state of the operating profile are fixed for and during each climatic condition (temperate, tropical and winter).

2) The total number of failed generator sets in the power reserve tool is assumed to be 1, as discussed in section 3.5.3.

3) The diesel engines in the candidate power system are assumed to be high speed engines, where the corresponding exhaust system is equipped with selective catalytic reduction technology to assure IMO tier III compliance.

4) Battery operation is constrained to between a maximum of 90% SoC and minimum of 20%.

5) When multiple generators are online they satisfy equal proportions of the load demand. Similarly when the switchboards are split, the total of the ships loads are shared equally between the switchboards.

6) The maximum allowable spinning reserve of the generator sets is limited to 100% rated power. It is acknowledged that DGs can operate at up to 110% of rated power (MTU, 2012) for infrequent
intervals (Daffey and Hodge, 2004) but this is not an ideal solution as this can reduce the operating periods between maintenance overhauls.

Modelling limitations

1) The models employed in this tool do not consider transient loading events and the impact these might have on fuel consumption, emissions and maintenance.

2) During State 1 all available DGs will be online with the ESS available to support pulsed power requirements (although there will be no LDEW pulsed loadings applied).

3) All power demands in the power system configurations are inclusive of losses.

4.3 Modelling tool 2: Quasi-steady state model for load levelling

4.3.1 Modelling purpose

The purpose of modelling tool 2 is twofold. First, to quantify the potential fuel consumption and exhaust GHG emissions when the battery ESS operates to load level a DG set in the candidate warship power system. Second, to explore the electrical performance on the main switchboard when operating the generator and battery ESS in parallel during load levelling. Consequently, this requires the simulation of the interaction between the ESS, ESS converter and the DG, AVR and governor, and the management system that determines the power split between the DG and battery ESS.

4.3.2 Modelling requirement

Modelling tool 2 simulates the interaction between one DG and one ESS as highlighted in the light grey shaded area in Fig. 4-3. In Table 3-3 in chapter 3 it was shown that, depending on the candidate ship operating state, the ship’s power system could operate in either split or closed bus tie configuration. This modelling tool therefore could be applied to when the ship’s loads are supplied by a single generator when the ship is operating in the patrol or transit states, or when the power system is operating in a split bus configuration during the manoeuvring state with one DG online on each switchboard. Therefore, the concept of modelling tool 2 can be applied to a large proportion of the candidate ship operating profiles. Manoeuvring, patrol and transit operating states cumulatively account for 81% of total operating time for both the ASW and GP profiles. Simulating the entire power system would substantially and unnecessarily increase the simulation run time.
Modelling

Fig. 4-3. Modelling requirement for modelling tool 2

Fig. 4-4 describes modelling tool 2 in more detail, only the breaker connecting the ESS interface with the main switchboard is simulated. A description of each of the constituent models and corresponding control systems are provided in sections 4.6 through 4.11. The constituent models of modelling tool 2 comprises the following models:

1) Battery ESS
2) VSC and LCL filter
3) Transformer
4) Breaker for connecting ESS to main switchboard
5) Power cables
6) 3 MW DG
7) Variable P,Q load

Fig. 4-4. System modelling diagram of modelling tool 2
4.3.3 Assumptions and limitations

The following assumptions and limitations apply to modelling tool 2.

Modelling assumptions

1) Battery operation is constrained to between a maximum of 90% SoC and minimum of 20%.

2) The battery ESS, VSC, filter, transformer, breaker, power cables and DG remain healthy during operation. Therefore, the results and conclusions drawn are based on healthy system operation only.

Modelling limitations

1) The accuracy of the ESS model in this tool is limited to examination of performance when the cells are operated at ambient temperatures between 15°C and 30°C.

2) The DG results are limited to electrical performance and mechanical power of the engine. Combustion dynamics are not included in the DG model.

3) The ship’s loads are aggregated as a programmable variable power factor lagging load. This reduces the simulation time associated with modelling a propulsion motor and variable speed drives, and has been used to model power system loads in the works of Ovrum and Bergh (2015) and Dinh et al. (2018) when modelling DG and ESS in parallel under quasi steady state conditions. The ship’s loads are represented by a variable resistive inductive load block to represent a variable lagging load.

4.4 Modelling tool 3: Dynamic model for LDEW operation

4.4.1 Modelling purpose

The purpose of modelling tool 3 is fourfold. First, to explore the transient impact of LDEW loads on the battery ESS and the DC/DC converter response. Second, the ability of the DC/DC converter to maintain QPS to the LDEW load. Third, to explore the ability of the battery ESS to satisfy a sustained four minute engagement under LDEW operation. Finally, to investigate the extent to which the useable SoC of the ESS is reduced when supplying power to the LDEW.

4.4.2 Modelling requirement

As detailed in chapter 3, while the LDEW is in operation, the battery ESS, DC/DC converter and LDEW are isolated from the wider ship distribution system. Since part of this research is to explore how the particular NMC based battery ESS, as selected in chapter 3, performs under the extreme loading conditions imparted by the LDEW, the requirement for modelling tool 3 is limited to the bounds of the candidate warship power system as highlighted by the light grey shaded area in Fig. 4-5.
This circuit could similarly apply to the aft switchboard installation or aligned to similar hybrid or full electric warship power system employing an LDEW with battery ESS power supply. This investigation approach therefore allows the comparison of the performance results of NMC based battery ESS in this research with different chemistries or electrical capabilities. The simulation based performance tool summarised by the simplified equivalent circuit model in Fig. 4-6, comprises the following models:

1) Battery ESS
2) Single stage DC/DC boost converter
3) 50 kW base load
4) LDEW pulsed load

The following assumptions and limitations apply to modelling tool 3.

**Modelling assumptions**

1) Battery operation is constrained between a maximum of 90% SoC and the minimum allowable SoC is 20%.
2) The battery system, DC/DC converter and LDEW remain healthy during operation. Cell, battery management system, power electronic or capacitor failures during operation of the LDEW would impact the performance of the system. Therefore the results and conclusions are based on healthy system operation only. Degraded performance from aging effects are considered in chapter 6, however the battery ESS is still considered to be in a healthy state.

3) The duty cycle of the load has been fixed to 40% to align with previous research in the field. However, as technology development and the concept of operations develop for LDEWs, the duty cycle may be adapted for future studies.

Modelling limitations

1) The accuracy of the ESS model in this tool is limited to examination of performance when the cells are operated at ambient temperatures between 15°C and 30°C.

4.4.4 Summary of modelling tools

Table 4-1 summarises the system responses of interest from the modelling tools from section 4.2 through 4.4. Details of the constituent components of the models will be described in the following sections. Each modelling tool was developed to produce the outputs required for analysis and the type of model required, this is summarised by Table 4-1.

<table>
<thead>
<tr>
<th>Modelling tool</th>
<th>ESS operating mode</th>
<th>Modelling tool outputs sought</th>
<th>Timescale of system response</th>
<th>Type of model required</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>Power reserve</td>
<td>Operating profile fuel consumption, engine running hours and exhaust GHG emissions.</td>
<td>Days</td>
<td>Steady-state</td>
</tr>
<tr>
<td>2</td>
<td>Load levelling</td>
<td>System QPS, fuel consumption and exhaust GHG emissions.</td>
<td>Seconds and minutes</td>
<td>Quasi-steady state</td>
</tr>
<tr>
<td>3</td>
<td>LDEW power supply</td>
<td>Battery transient performance, system dynamic response and system QPS.</td>
<td>Second and sub-second</td>
<td>Dynamic</td>
</tr>
</tbody>
</table>

4.5 Software selection

The software package selected to build the modelling tools in this research was MATLAB/Simulink. There are other programming languages available such as Python or C, however MATLAB provides alignment with the SimPowerSystems component of MATLAB/Simulink. This facilitated the simulation of electrical power system components for modelling tools 2 and 3. Modelling tool 1 was developed using a script based approach in MATLAB. Other packages such as Power Systems Computer Aided Design were available.
However, to align the development effort of the tools in this research MATLAB/Simulink was considered better suited.

### 4.6 Battery model

The battery model was developed to be representative of a 4.2 V, 64 Ah Li-ion polymer pouch type cell with a lithium nickel manganese cobalt cathode and graphite anode as is used in the Corvus Energy Orca system. Table 4-2 summarises the electrical specifications of the corresponding battery cell, module and string. The cell model was scaled in series and parallel to represent the battery ESS to meet the power demand of the LDEW. For higher fidelity modelling of the battery system, the cells could be modelled separately as conducted by Roscher et al. (2011). However, modelling the 2,112 cells separately was deemed inappropriate due to the associated computational complexity with cell monitoring and balancing. The limitation of a single cell representation of the system is that cell and module voltage variation and thermal distribution are not captured. However, this was addressed by conducting module level validation to capture the level of error in scaling the cell model, the results of this are detailed in section 4.13.1.

As discussed in chapter 3, the steady state power limit is a good starting point from which to assess the minimum number of strings required. This is plotted in Fig. 4-7 using (3.9) over the SoC operating window of the battery ESS. Fig. 4-7 demonstrates that four strings are required to meet the 2 MW LDEW load, assuming 90% efficiency of the boost converter (Kazimierczuk, 2016). Each string is independently fused, and this limits the number of strings that can be added in parallel to four, beyond this supplementary protection in the form of DC breakers is required due to the fault contribution of 9.3 kA per string of the battery ESS.

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Cell</th>
<th>Module</th>
<th>String</th>
<th>Unit</th>
</tr>
</thead>
<tbody>
<tr>
<td>Nominal capacity (1C rate)</td>
<td>64</td>
<td>128</td>
<td>128</td>
<td>Ah</td>
</tr>
<tr>
<td>Peak voltage</td>
<td>4.2</td>
<td>50</td>
<td>1,100</td>
<td>V</td>
</tr>
<tr>
<td>Nominal voltage</td>
<td>3.7</td>
<td>45</td>
<td>980</td>
<td>V</td>
</tr>
<tr>
<td>Cut-off voltage</td>
<td>3.0</td>
<td>36</td>
<td>800</td>
<td>V</td>
</tr>
<tr>
<td>Peak cont. discharge current (6C)</td>
<td>384</td>
<td>768</td>
<td>768</td>
<td>A</td>
</tr>
<tr>
<td>Peak charge current (3C)</td>
<td>192</td>
<td>384</td>
<td>384</td>
<td>A</td>
</tr>
<tr>
<td>Cells</td>
<td>1</td>
<td>24 (12x2p)</td>
<td>528</td>
<td></td>
</tr>
</tbody>
</table>
4.6.1 Cell dynamic model

The cell dynamic model is used in modelling tool 2 and 3. Li and Mazzola (2013) categorise battery models into either electrochemical or behavioural models. Electrochemical models are comprehensive, detailed and intended for the physical battery structure and material design of the cell, therefore are not suitable for system level simulation due to the corresponding computational expense (Li and Mazzola, 2013). Behavioural models include mathematical and electrical circuit forms, where the former comprise formulas derived from curve fitting to experimental results. The complexity and accuracy of equivalent circuit models are between that of the electrochemical and mathematical battery models (Chen and Mora, 2006; Li and Mazzola, 2013), therefore the equivalent circuit model approach is more appropriate for system level investigation in this research. In this work, the equivalent circuit model is scaled in series and parallel to resemble the battery ESS with four strings as shown in Fig. 4-8. The blue lines represent the current and voltage sensor measurements. These measurements are scaled as required to represent the number of cells, modules and strings as shown in the schematic. Each string has the characteristics as detailed in Table 4-2. For the purpose of this model, inductance of the physical arrangement is assumed negligible. Overcurrent and undervoltage trips are included in the cell level model to represent the practical limits that would be implemented in the actual battery management system. These are set at 384 A and 3 V respectively.

Fig. 4-8. Scaling model of single cell model to module level, and module to ESS level

Key

<table>
<thead>
<tr>
<th>Symbol</th>
<th>Description</th>
</tr>
</thead>
<tbody>
<tr>
<td>( \diamond )</td>
<td>Controllable current source</td>
</tr>
<tr>
<td>( \bigtriangleup )</td>
<td>Controllable voltage source</td>
</tr>
<tr>
<td>( \bigtriangledown )</td>
<td>Current sensor</td>
</tr>
<tr>
<td>( \bigvee )</td>
<td>Voltage sensor</td>
</tr>
<tr>
<td>( \bigtriangledown )</td>
<td>Gain</td>
</tr>
</tbody>
</table>

Fig. 4-7. Steady state power limit of the battery ESS as a function of the number of strings
The cell model was developed to represent a second order behavioural Thévenin equivalent circuit model. The order of the model defines the resolution of the dynamic, non-linear voltage response of the battery under charge or discharge conditions. The order number refers to the number of Resistor-Capacitor (RC) pairs in the Thévenin equivalent circuit model. The order choice depends on the fidelity required and modelling objective, typically one to three RC pairs are used (Hentunen et al., 2014). Two RC pairs offer a good balance between accuracy and complexity (Chen and Mora, 2006; Li and Mazzola, 2013). Moreover, a second order model meets the modelling requirement to capture the transient response of the battery and therefore was selected for use in this work. The first RC pair is representative of the fast dynamics of the battery, where the time constant is in the order of less than one second. The second RC pair denotes the slower dynamics of the cell caused by the diffusion processes in the electrolyte (Mesbahi et al., 2016). The second time constant is in the hundreds of seconds (Hentunen et al., 2014).

The cell equivalent circuit model is configured as shown in Fig. 4-9. The actual Simulink model schematic of the cell and battery ESS can be found in Appendix B. It includes open circuit voltage $u_{OCV}$, internal resistance $R_0$, transient circuit voltages, $u_1$ and $u_2$, $R_1$ and $R_2$ are the dynamic resistances, $C_1$ and $C_2$ are the corresponding dynamic capacitances. The dynamic resistances and capacitances denote the non-linear complex impedance RC pairs. These parameters do not embody the battery internal components per se, rather they signify a state-variable representation of the battery from the perspective of the cell terminals. Each of these parameters in Fig. 4-9 varies with the state of charge, $s_Q$. The parameters are mapped using high order polynomial functions that are dependent on SoC. Estimating SoC accurately is challenging as SoC estimation is influenced by temperature and battery aging (Hu et al., 2012; Waag et al., 2014). Although susceptible to SoC drift if there are errors in current measurement, coulomb counting is a robust and simple method of measuring SoC (Kalikatzarakis et al., 2018). This was implemented in the model using (4.1). The dynamic model of the cell is summarised mathematically by (4.2) and (4.3).

$$s_Q = s_{Q0} - \frac{1}{Q_{batt}} \int i_b dt$$ \hspace{1cm} (4.1)

$$u_b = u_{OC}(s_Q) - R_a i_t - u_1(s_Q) - u_2(s_Q)$$ \hspace{1cm} (4.2)

$$\begin{bmatrix} \dot{u}_1 \\ \dot{u}_2 \\ \dot{s}_Q \end{bmatrix} = \begin{bmatrix} -1/R_1C_1 & 0 & 0 \\ 0 & -1/R_2C_2 & 0 \\ 0 & 0 & 1/Q \end{bmatrix} \begin{bmatrix} u_1 \\ u_2 \\ s_Q \end{bmatrix} + \begin{bmatrix} 1/C_1 \\ 1/C_2 \\ 1/Q \end{bmatrix} i_b$$ \hspace{1cm} (4.3)

where $s_{Q0}$ is the initial state of charge, $Q_{batt}$ is the useable battery capacity, $u_b$ and $i_b$ are the battery terminal voltage and current respectively. The polynomial functions of the state variables were parameterised from experimental test data. This process is detailed in the following sub-section.
4.6.2 Parameter extraction method

Equivalent circuit model open circuit voltage, internal resistance and transient circuit RC time constants can be parameterised using two methods (Hentunen et al., 2014). Either by using electrochemical impedance spectroscopy in the frequency domain (Bellache et al., 2018) or through analysis of experimental voltage and current time-series data from pulsed charge and discharge current pulse tests. Examples of the latter approach can be found in the works of Chen and Mora (2006), Li and Mazzola (2013) and Hentunen et al. (2014). The time domain parameter extraction method presented by Hentunen et al. (2014) was followed to extract the parameters for the equivalent circuit model in this research. First, because the method presented in their work is a fast, analytical method that negates the need for iterative simulations to optimise the parameters. Second, because experimental time series pulsed discharge experimental data was available and was provided by Corvus Energy for the 4.2 V, 64 Ah Li-ion polymer pouch type cell used in their Orca system. Table 4-3 details the experimental results provided by Corvus Energy to develop and validate the battery model developed in this research. A letter from Corvus Energy confirming the authenticity of the data and model validation is provided in Appendix A.

Table 4-3: Battery data provided by Corvus Energy used to develop and the modelling purpose for use in this research.

<table>
<thead>
<tr>
<th>Experimental data</th>
<th>Modelling purpose</th>
</tr>
</thead>
<tbody>
<tr>
<td>Cell 0.5C, 5% pulsed discharge from 100% to 0% SoC at 15°C, 22°C and 30°C</td>
<td>Develop equivalent circuit model</td>
</tr>
<tr>
<td>0.5 C, 1C, 2C, 3C and 6C constant current cell discharge</td>
<td>Validation and develop efficiency model</td>
</tr>
<tr>
<td>0.5C, 1C, 2C and 3C constant current, constant voltage cell charge</td>
<td>Validation and develop efficiency model</td>
</tr>
<tr>
<td>Module 2C charge and discharge</td>
<td>Validation</td>
</tr>
<tr>
<td>Module 4.5C discharge</td>
<td>Validation</td>
</tr>
</tbody>
</table>

The parameter extraction method analyses the voltage relaxation characteristics during the rest times of the experimental pulsed discharge test. The pulsed discharge test comprises a sequence of current pulses with
a rest period between pulses. The rest period for such an experiment is typically from 5 to 60 minutes, with pulse rate from 2% to 20% (Hentunen et al., 2014; Stroe et al., 2017). Corvus Energy conducted three pulsed discharge tests on a Li-ion cell whose characteristics are described in Table 4-2. Three constant temperature pulsed discharge tests were executed in a test chamber maintained at 15°C, 22°C and 30°C. The cell simulation model was parameterised using the 22°C pulsed discharge test results. This is because it is desirable to maintain ambient thermal conditions in battery modules at or close to 25°C to provide optimal conditions for Li-ion battery operation (Al-Zareer et al., 2017; de Hoog et al., 2018). At temperatures above 25°C, the cathode in NMC cells can undergo degeneration and an SEI layer grows on the anode (Birkl et al., 2017; de Hoog et al., 2017; Jalkanen et al., 2015). The effect of conducting the pulsed discharge experiment at 15°C and 30°C on the cell are compared and assessed in section 4.6.4.

The pulsed discharge experiments were conducted at 0.5C, 5% pulse rate from 100% to 0% SoC, with 30 minute rest period between pulses. The experimental results for the pulsed discharge test at 22°C are shown in Fig. 4-10. The experiment discharged the cell from 100% to 0% SoC at 5% intervals, with the current pulse discharge peak at 0.5 C as shown by the secondary y-axis. The plot in Fig. 4-10 shows that the characteristics of the cell voltage response over the SoC range of the battery. The voltage response, both during and after each current pulse, varies in amplitude and dv/dt. This is due to the electrochemical behaviour in the cell, hence each voltage relaxation period is analysed separately in the parameter extraction method.

![Pulse discharge experiment at 0.5C rate, 5% pulse rate and 30 minute rest period, from 100% to 0% SoC at 22°C](image)

Hentunen et al. (2014) describes the extraction method in detail. The pertinent stages as described here are used in this research. The method first separates the voltage response into discrete sections defined by the start and end times of each current pulse, so that each voltage relaxation period following a current pulse can be analysed individually. The rest period under investigation is then partitioned into sections to identify the corresponding RC parameters for that particular SoC.

The internal resistance, $R_0$ of the cell is defined by (4.4) at the beginning or end of a current pulse.

$$R_0 = \frac{\Delta U}{\Delta I}$$ (4.4)
where $\Delta U$ is the voltage difference during the current pulse, and $\Delta I$ is the amplitude of the current pulse. The battery terminal voltage $u_b$ during the relaxation period after a current pulse is given by (4.5).

$$u_b = u_{OCV} - \sum_{i=1}^{n} U_i e^{-t/\tau_i}$$  \hspace{1cm} (4.5)$$

where $n$ is the order number of the equivalent circuit, $U_i$ is the initial voltage of the ith parallel RC branch, $\tau_i = R_i C_i$ is the corresponding extracted time constant from experimental data and $t$ is time. During the rest period the terminal voltage decays at a rate determined by the time constants until $u_b$ equals $V_{OCV}$. The initial time instant of the rest period is at the point where the discharge current of the cell reaches zero, and after the instantaneous voltage drop caused by the internal resistance. The transient circuit voltage parameters, $U_i$ and $\tau_i$ are estimated after the instantaneous voltage drop has occurred in the rest period. For the two RC branches in this research, the experimental transient circuit voltage, $u_t$ is expressed by (4.6).

$$u_t = U_1 e^{-t/\tau_1} + U_2 e^{-t/\tau_2}$$  \hspace{1cm} (4.6)$$

The rest period following a current pulse is then partitioned into two windows to extract the short and long time constants. Fig. 4-11 shows an example of the partitioning of the rest period when the battery is at 95% SoC. The time instants for the rest period in Fig. 4-11 use the notation described by Hentunen et al. (2014), and are as follows:

1) $t_{cpe}$ when current pulse ends and current falls.
2) $t_0$ when current has fallen to zero- beginning of rest period. For analysis of each rest period $t_0 = 0$ s.
3) $t_{11}$ start of first time window, $t_{11} = t_0$
4) $t_{12}$ end of first window.
5) $t_{21}$ start of second window.
6) $t_{22}$ end of second window.
7) $t_{end}$ end of rest period.
Fig. 4-11. Voltage relaxation characteristics during the pulsed discharge test at 95% SoC. The time instances are shown during the rest period following a current pulse.

Now that the rest period has been defined, the next step of the method is to define the second window time instants, $t_{21}$ and $t_{22}$ to extract the second window time constant. The second window is assumed to be in the order of minutes, where it is also assumed that the voltage of the first, fast, time constant branch $U_1$ has reduced to zero. Therefore the expression for $u_\tau$ can be rewritten as:

$$u_\tau(t) = u_\tau(t_{21})e^{-\frac{(t-t_{21})}{\tau_2}} \text{ for } t > t_{21}, \quad (4.7)$$

where $u_\tau(t_{21})$ is the voltage at the start of the second time window. Therefore, by defining $t = t_{22}$, the time constant for the second branch can be predicted by (4.8).

$$\hat{\tau}_2 = \frac{t_{22} - t_{21}}{\ln\frac{u(t_{21})}{u(t_{22})}} \text{ for } u_\tau \neq 0 \quad (4.8)$$

where $\hat{\tau}_2$ is the predicted time constant of the second window and $\hat{}$ donates the predicted value. Using $\hat{\tau}_2$, the second branch voltage at $t_0$ can be calculated using:

$$\bar{U}_2 = u_\tau(t_{21})e^{\frac{t_{21}}{\hat{\tau}_2}} \quad (4.9)$$

The predicted voltage with respect to time is therefore:

$$\hat{u}_2(t) = \bar{U}_2e^{\frac{-t}{\hat{\tau}_2}} \text{ for } t \geq 0 \text{ s} \quad (4.10)$$

Subsequently, knowledge of $\bar{U}_2$ and $\hat{\tau}_2$ allows the extraction of the short time constant. First, the predicted long time constant branch voltage must be subtracted from the experimental voltage response of the cell using (4.11).
\[ u_{\tau, \text{mod}}(t) = u_{\tau}(t) - \hat{u}_2(t) \]  
\[ \text{(4.11)} \]

where subscript mod donates modified. The transient circuit voltage is then expressed as:

\[ u_{\tau, \text{mod}}(t) = u_{\tau, \text{mod}}(t_{11})e^{-\frac{(t-t_{11})}{\tau_1}} \text{ for } t \geq t_{11} \text{ s} \]
\[ \text{(4.12)} \]

Subsequently

\[ \hat{\tau}_1 = \frac{t_{12}-t_{11}}{\ln\left(\frac{u_{\tau}(t_{11})}{u_{\tau}(t_{12})}\right)} \text{ for } u_{\tau, \text{mod}} \neq 0 \]
\[ \text{(4.13)} \]

The initial voltage of the first branch at \( t_0 \) is then:

\[ \hat{U}_1 = u_{\tau}(t_{11})e^{\frac{t_{11}}{\tau_1}} \]
\[ \text{(4.14)} \]

The predicted voltage of the first RC branch is then:

\[ \hat{u}_1(t) = \hat{U}_1 e^{-\frac{t}{\tau_1}} \text{ for } t \geq 0 \text{ s} \]
\[ \text{(4.15)} \]

It follows that the predicted transient circuit voltage is expressed as:

\[ \hat{u}_{\tau} = \hat{U}_1 e^{-\frac{t}{\tau_1}} + \hat{U}_2 e^{-\frac{t}{\tau_2}} \]
\[ \text{(4.16)} \]

The resistance, \( R_i \) and capacitance \( C_i \) values can then be extracted knowing the current pulse amplitude, \( I_{CP} \) and duration \( \tau_{CP} \), using (4.17) and (4.18).

\[ R_i = \frac{\hat{U}_i}{I_{CP}\left(1 - e^{-\frac{\tau_{CP}}{\tau_1}}\right)} \]
\[ \text{(4.17)} \]
The method described above was compiled as a script in MATLAB to automate the parameter extraction process. The script is provided in Appendix C.

4.6.3 Model parameter maps

The time instances used for parameter extraction were \( t_0 = t_{11} = 0 \text{s}, t_{12} = 12 \text{ s}, t_{21} = 240 \text{ s}, t_{22} = 600 \text{ s}. \) These were selected based on the values used in Hentunen et al. (2014) who conducted similar pulsed discharge experiments with 30 minute rest periods for an NMC cell, with the errors returned by their developed model being deemed acceptably low. The MATLAB script in Appendix C was used to analyse the raw data provided by Corvus Energy for the pulsed discharge experiment. An example of one of the current pulse voltage responses is exhibited in Fig. 4-12(a), showing the voltage drop with discharge current pulse, and subsequent transient relaxation period immediately following the current pulse. Fig. 4-12(b) shows an example of extracting the transient circuit voltage, the time axis has been shifted to the origin at commencement of the relaxation period. \( u_\tau \) was subtracted from the OCV to invert the voltage curve. The plot demonstrates the decay of the short and long time constant components of \( u_\tau \), as well as the accuracy of the fit to the experimental data. The accuracy of the extraction method is validated for the example pulse in Fig. 4-12 (c).
The results for each parameter were then plotted against SoC. As the RC parameters were fixed at each 5% SoC interval, the parameters were then up-sampled by a factor of 10 using a shape preserving interpolation method to generate the parameter maps plotted in Fig. 4-13 through Fig. 4-16. This post processing method results in smoother parameter maps. The parameter maps of the RC branches show that the capacitive behaviour dominates the time constants in the system. It should be noted that the time instances used in the extraction process were varied to assess the accuracy of the predicted against experimental results. However, changing the instances from the values used in Hentunen et al. (2014) increased the error level, verifying the decision to select them based on the referenced work.

In the actual simulation model of the cell, these parameter maps are implemented as high order polynomial functions. These are included in Appendix D. Look-up tables were considered as an alternative to polynomial functions. However, the former could introduce numerical instability (Ahmed, 2016), moreover as the battery model is integrated with a system comprising power electronics, the look-up tables would introduce time constants that could increase the stiffness of the simulation and reduce simulation speed.
Fig. 4-13. Parameter map of $R_0$ at 22°C

Fig. 4-14. Parameter maps of (a) $\tau_1$ and (b) $\tau_2$ at 22°C

Fig. 4-15. Parameter maps of $R_1$ and $R_2$ at 22°C
4.6.4 Cell temperature effects

The pulsed discharge experiment for the three different temperatures are shown in Fig. 4-17. The response of the cell shows that the voltage drop is greatest when conducted at 15°C. Conversely, the voltage is highest at 30°C. This leads to a reduction in capacity at low temperatures and an increase at higher temperatures. This effect is validated by the time it takes to reach the end of discharge and cut off voltage of the cell. The cutoff voltage is the termination criteria for a pulsed discharge experiment (Hentunen et al., 2014). The lower limit cut off voltage is exceeded after 700 minutes at 15°C, 703 minutes at 22°C and 736 minutes at 30°C. The latter is over 30 minutes later because an additional current pulse is provided by the cell. Recordings of the temperature in the test chamber are included in Appendix E for reference.
An increase in the temperature of the cell results in the decrease in viscosity and increase in activity of the electrolyte, which may strengthen the migration effect and ion diffusion in the cell (Doughty et al., 2005). Therefore, it is expected that due to Arrhenius equation (4.19), which pertains to the relationship between the chemical reactions and temperature of the cell, the internal resistance would be lower at higher temperature (Jouhara et al., 2019).

\[
k = A e^{-\frac{E_a}{RT}}
\]

(4.19)

where \(k\) is the rate of chemical reaction, \(A\) is a constant, \(T\) is temperature, \(E_a\) is the activation energy of the chemical process and \(R\) is the gas constant (8.314 J mol K\(^{-1}\)) (Root, 2010). Therefore, with higher temperature, the rate of reactions increases and the internal resistance of the cell decreases. This is reflected in the resistance against SoC plot of the cell in Fig. 4-18, calculated using (4.4). This explains the voltage response of the cell in Fig. 4-17.

![Graph showing internal resistance against SoC for different temperatures.](image)

**Fig. 4-18.** Measured internal resistance during 5% pulsed discharge test at 15°C, 22°C and 30°C

### 4.6.5 Steady state efficiency model

The empirical efficiency model of the battery ESS, used in modelling tool 1, comprises an exothermic model of the cells in the system that captures the heat generated by the cells as a consequence of their internal resistance. The model does not take into account the losses that may be induced by aging affects or other auxiliary losses of the ESS such as the battery management system and contact resistances These were assumed negilbile. This method expands on the approach of Barrera-Cardenas et al. (2019). Instead of adopting a constant efficiency value for charging and a constant value for discharging. The method used here adopts an efficiency map approach by taking into account the internal resistance of the cells as a function of current and SOC.

The first step was to determine the internal resistance of the cell over its SoC range using the OCV and voltage charge/discharge curves provided from the manufacturer’s data. This was determined by calculating the voltage drop caused by the current drawn from the battery over the SoC range of the cell using (4.20). The internal resistance map for the cell is plotted in Fig. 4-19. As expected it can be seen that with increasing discharge current there is lower internal resistance relative to that exhibited at low current rates.
\[ R_0(SoC, I) = \frac{|u_{OCV}(SoC) - V(SoC, I)|}{I} \]  
(4.20)

The heat generation, \( Q_{cell} \), from the cell is determined using (4.21), the corresponding losses for the cell are shown in Fig. 4-20. The peak loss in Fig. 4-20 is 175 W at 10% SoC. During the useable SoC window of 20-90% the cell exothermic losses are below 130 W.

\[ Q_{cell}(SoC, I) = R_0(SoC, I)I^2 \]  
(4.21)

The efficiency map of the cell can be generated using (4.22) during charge or discharge. The efficiency map of the cell during discharge is plotted in Fig. 4-21. Fig. 4-21 shows that the cell is more efficient at low current rates and in the nominal voltage range of the cell (3.7 V). At 1C this is approximately 99%, while at high currents and low SoC the efficiency can be as low as 90% at 10% SoC. The efficiency map of the cell was then scaled based on the discharge current of the system and a polynomial function was fit to the efficiency map to calculate the battery ESS losses during discharge. This process was repeated for charging efficiency as shown in Fig. 4-22. The polynomial functions are included in Appendix F.
\[
\eta_{\text{cell}}(\text{SoC}, I) = \frac{P_{\text{cell}}(\text{SoC}, I)}{P_{\text{cell}}(\text{SoC}, I) + Q_{\text{cell}}(\text{SoC}, I)}
\]

(4.22)

Fig. 4-21. Efficiency map of candidate NMC cell with SoC and discharge current

Fig. 4-22. Efficiency map of candidate NMC cell with SoC and charging current in the constant current charging phase

4.7 VSC DC/AC converter model, LCL filter and control system design

The VSC previously described in section 3.6.2, and shown in Fig. 4-23 is employed using the SimPowerSystems blockset model of a Universal Bridge, a full description of which is offered by The MathWorks, Inc (2018c). The IGBT data that is used to characterise the switching devices of the VSC steady state and dynamic models are representative of the 5SNA IGBT module from ABB (2014). This particular IGBT module has a maximum DC collector current of 3,600 A. These IGBT modules are also used in the DC/DC converter model described in section 4.12. These modules were selected as they are capable of delivering the maximum battery discharge current of 3,070 A.

The switching of the IGBT devices in the VSC is managed using the sinusoidal PWM control strategy to control the amplitude phase and frequency of the VSC. A full description of this PWM strategy is offered
by Espinoza (2007) and a brief description is provided here. This strategy compares the desired reference three phase AC voltage output waveform with a high frequency triangular waveform, known as the carrier signal, to define the on and off states of the switches in each leg of the VSC. Where the first leg is SW1 and SW2, the second is SW3 and SW4, the third is SW5 and SW6. When the reference waveform is greater than the carrier the upper switching device conducts (e.g. SW1), and the lower switching device (SW2) is off. The opposite applies when the reference waveform is lower than the carrier. This principle is demonstrated in Fig. 4-24 for phase A of the two level VSC in Fig. 4-23.

Fig. 4-23. Two level VSC model schematic

![Two level VSC model schematic](image)

Fig. 4-24. Sinusoidal PWM principle for leg 1 of the two level VSC in Fig. 4-23 with modulation index of 0.8, showing (a) carrier and reference waveforms (b) Switch 1 pulse signal and (c) Switch 2 pulse

The primary control structure of the VSC is shown in Fig. 4-25. In this work the converter is controlled to emulate an AC voltage source connected through a link impedance to the main switchboard. As such, the primary control structure comprises droop control and inner control loops. The function and characterisation of the inner control loops highlighted by the red dashed box in Fig. 4-25 are described in detail in the following sub-section. Droop control is discussed in section 4.10.3.
4.7.1 Inner control loops

The inner control loops of the VSC highlighted in Fig. 4-25 comprise two loops, an outer voltage loop and an inner current loop. The VSC response is controlled in the dq reference frame. The premise of the dq reference frame is to transform the three-phase references for current and voltage to a frame that synchronously rotates with the frequency of the power system. This allows the three-phase quantities to be represented as DC quantities (under steady state conditions) that can be independently controlled. The benefit of this is that the active and reactive power of the VSC can be independently controlled. The active and reactive power in the dq frame are described mathematically by (4.23) and (4.24) respectively.

\[
P(t) = \frac{3}{2} [v_d(t)i_d(t) + v_q(t)i_q(t)] \tag{4.23}
\]

\[
Q(t) = \frac{3}{2} [-v_d(t)i_q(t) + v_q(t)i_d(t)] \tag{4.24}
\]

where \(i_d\) and \(i_q\) are the direct and quadrature components of the inductor current. \(v_d\) and \(v_q\) are the direct and quadrature components of the capacitor voltage.

A more detailed schematic of the inner loop control system is shown in Fig. 4-26. The actual Simulink model schematic can be found in Appendix G. The outer voltage loop is responsible for regulating the output voltage across the LCL filter’s capacitor, \(V_c\) to attain the AC reference voltage for the primary side of the transformer. The inner control loop regulates the current to be supplied by the battery or drawn from the grid by controlling the inductor current \(I_L\). The phase angle between the \(V_c\) and \(I_L\) are controlled to permit bi-directional power flow of the VSC. When \(I_L\) is out of phase with \(V_c\), the VSC is in rectifier mode, where the battery is charged from the ship’s generators. When the \(I_L\) is in phase with \(V_c\), the VSC is in inverter mode. The control structure uses a decoupling current and voltage network to improve the performance of the independent axis quantities managed by the control loops (Timbus et al., 2009). In this model the location of the current sensor permits the current limiting capability for protecting the converter.
and battery system that would be present in the practical implementation of the control system of the VSC.

![LCL filter schematic](image)

**Fig. 4-26.** VSC inner loop control system schematic with dual loop PI control in the dq axis reference frame

### LCL filter characterisation

The LCL filter dampens the distortion of the output voltage and current sinusoidal waveforms from the converter and reduces the high frequency harmonics induced by the VSC switching frequency. The factors that govern the design of the LCL filter are minimising the installed reactive power of the LCL filter, minimise cost and weight, ensure robustness of the filter during different conditions, the effect of the damping resistor on attenuation capability, and avoidance of resonances (Liserre et al., 2005; Teodorescu et al., 2011). The design of the LCL filter follows the procedure as described by Liserre et al. (2005), that uses the power rating of the converter, system frequency and VSC switching frequency as inputs. The following process was used to design the parameters of the LCL filter at the output of the VSC and the controller gains in the inner loop controllers:

1. Define equivalent circuit of the filter
2. Define input parameters for calculations
3. Characterise filter components and filter damping
4. Derive LCL filter transfer function
5. Define current control loop transfer function for control design
6. Define voltage control loop transfer function for control design

### Per phase equivalent circuit

The per phase equivalent circuit of the LCL filter is shown in Fig. 4-27 for the $h^{th}$ harmonic, neglecting $R_1$ in Fig. 4-26 above. $i(h)$ is the current provided by the VSC, while $i_g(h)$ is the harmonic current injected to the grid. $L_1$ is the VSC side inductor, $L_g$ is the grid side inductance, which is the sum of the second inductor
L₂ and transformer inductance Lₜ. C_f is the LCL filter capacitor.

\[
L_g = L_2 + L_T
\]

\[
L_g = L_2 + L_T
\]

\[
L_1
\]

\[
C_f
\]

\[
i_g(h)
\]

\[
i(h)
\]

Fig. 4-27. Per phase equivalent circuit of the LCL filter

**Input parameters**

The filter design input parameters are provided in Table 4-4. The 480 V line-to-line voltage, \(V_{L-L}\) was determined using equation (3.1) in chapter 3, with a modulation index of 0.8 at nominal voltage of the battery ESS. At the cutoff voltage of the battery the modulation index becomes 1. Therefore, the PWM control strategy operates in the linear modulation for the range of battery ESS voltage. The 2 kHz switching frequency was selected as this is typical for medium power applications in shipboard power systems (Bosich et al., 2017).

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
<th>Unit</th>
</tr>
</thead>
<tbody>
<tr>
<td>Nominal DC voltage, (V_{DC})</td>
<td>980</td>
<td>V</td>
</tr>
<tr>
<td>Grid line-line voltage, (V_{L-L})</td>
<td>480</td>
<td>V</td>
</tr>
<tr>
<td>VSC power rating, (P_n)</td>
<td>3.0</td>
<td>MW</td>
</tr>
<tr>
<td>Power factor</td>
<td>0.8</td>
<td></td>
</tr>
<tr>
<td>Grid frequency, (f)</td>
<td>60</td>
<td>Hz</td>
</tr>
<tr>
<td>Switching frequency, (f_{sw})</td>
<td>2</td>
<td>kHz</td>
</tr>
</tbody>
</table>

**Filter parameter characterisation process**

The first step in the filter component characterisation process is to define the base impedance:

\[
Z_b = \frac{V_{L-L}^2}{P_n} = 0.077 \, \Omega
\]

The converter side inductance is characterised to minimise the rated current ripple (Vasconcelos et al., 2007), in this case this was selected as 5%. The current ripple is:

\[
\Delta I_{max} = \Delta I_L \frac{P_n\sqrt{3}}{3V_{ph}} = 300 \, A
\]

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To achieve a 5% current ripple, the corresponding converter side inductance is calculated as:

\[ L_1 = \frac{V_{dc}}{16f_{sw} \Delta I_{max}} = 102 \mu H \]  

(4.27)

The filter capacitance is based upon the reactive power absorbed by the filter under rated conditions, given by (4.28), where \( x \) is the percentage of reactive power absorbed, this is set at 5% (Liserre et al., 2005), and \( C_b \) is the base capacitance, determined by (4.29).

\[ C_f = xC_b = 1.7 \ mF \]  

(4.28)

\[ C_b = \frac{1}{2\pi fZ_b} = 34.5 \ mF \]  

(4.29)

The current ripple attenuation is calculated by using the assumption that at high frequency the converter is a harmonic generator, and the grid is a short circuit (Liserre et al., 2005). The current ripple attenuation achieved from the converter to the grid via the LCL filter is thus determined using the ratio of the harmonic current generated by the inverter, \( i(h_{sw}) \) and the harmonic current injected to the grid \( i_g(h_{sw}) \):

\[ \frac{i_g(h_{sw})}{i(h_{sw})} = \frac{1}{1 + r(1 - L_1C_f \omega_{sw}^2)} \]  

(4.30)

where \( r \) is the desired ripple reduction factor. Knowledge of \( r \) allows the calculation of the grid side inductor, \( L_g \) using (4.31), neglecting losses and damping of the filter.

\[ L_g = rL_1 \]  

(4.31)

The desired current ripple attenuation with respect to the converter side is 10%, therefore by plotting \( i_g(h_{sw})/i(h_{sw}) \) against \( r \) (Fig. 4-28). Ten percent ripple reduction correlates to an \( r \) value of 0.4 as shown by point A in Fig. 4-28, therefore \( L_g \) is 40 \( \mu H \).
The resonant frequency requirement of the filter is to be between ten times the grid frequency (600Hz) and one-half of the switching frequency of the converter (1 kHz) (Liserre et al., 2005). This avoids resonance at lower and upper parts of the harmonic spectrum. The resonant frequency of the filter, $f_{\text{res}}$ is computed using (4.32).

$$f_{\text{res}} = \frac{1}{2\pi} \sqrt{\frac{L_1 + L_g}{L_1 L_g CF}} = 751 \text{ Hz}$$

Therefore, the filter design at the resonant frequency of 751 Hz satisfies the design requirement. It is noted that whilst a larger value of $r$ would reduce the penetration of ripple current to the grid, this would be at the expense of larger inductor size and cost. Therefore, 10% ripple attenuation is considered adequate for this application.

**Filter damping**

A resistor in series with the capacitor provides passive damping. This is designed to be one third of the impedance of the filter capacitor at resonant frequency (Liserre et al., 2005; Vasconcelos et al., 2007). As discussed by Teodorescu et al. (2011b), the introduction of passive damping in the LCL filter does not impact the control dynamics.

$$R_d = \frac{1}{3\omega_{\text{res}} CF} = 0.04 \Omega$$

An inductor in parallel with the damping resistor, specified at a lower impedance than the damping resistor for frequencies below the filter resonant frequency, reduces the filter losses. The damping inductor rating is specified using (4.34) (Peña-Alzola et al., 2013; Vasconcelos et al., 2007). At fundamental frequency, $\omega = \omega_f$, therefore, by using (4.34), the inductor is designed to provide a low impedance path as $L_d \omega_f \ll R_d$. Therefore the low frequency components will flow through the low impedance branch,
reducing the power losses of the filter at the fundamental frequency (Gomes et al., 2018).

\[ L_d = \frac{R_d}{\omega_{res}} = 8.7 \mu H \]  

(4.34)

The final per-phase equivalent circuit and parameters of the LCL filter design are given in Fig. 4-29 and Table 4-5 respectively.

![Fig. 4-29. LCL filter with parallel inductor-resistor damping](image)

Table 4-5. per phase equivalent circuit LCL filter parameters

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
<th>Unit</th>
</tr>
</thead>
<tbody>
<tr>
<td>Converter side inductance, ( L_1 )</td>
<td>102</td>
<td>( \mu H )</td>
</tr>
<tr>
<td>LCL capacitor, ( C_f )</td>
<td>1.7</td>
<td>( mF )</td>
</tr>
<tr>
<td>Grid side inductance, ( L_g )</td>
<td>40</td>
<td>( \mu H )</td>
</tr>
<tr>
<td>Damping resistor ( R_d )</td>
<td>0.04</td>
<td>( \Omega )</td>
</tr>
<tr>
<td>Damping inductor, ( L_d )</td>
<td>8.7</td>
<td>( \mu H )</td>
</tr>
</tbody>
</table>

**LCL filter transfer functions**

The transfer function of the inverter output voltage to the grid output current for the damped filter in the s-domain is given in (4.35), the details of the derivation are provided in Appendix H. The corresponding system frequency response is plotted in Fig. 4-30. Note that the addition of the series resistor in the capacitive shunt branch of the filter eliminates the gain spike of the undamped filter at resonant frequency (point A in Fig. 4-30), and reduces the roll-off of the third order filter from -60 dB/decade to -40 dB/decade. The bode plot shows the filter attenuating frequencies above \( f_{res} \).
\[
\frac{i_y}{v} = \frac{R_d C_f s + 1}{C_f L_1 L_y s^3 + (L_1 R_d + L_y R_d + L_y R_1) C_f s^2 + (L_1 + L_y + R_1 R_d) C_f s}
\]  
(4.35)

\begin{figure}[h]
\centering
\includegraphics[width=\textwidth]{bode_plot.png}
\caption{Bode plot of the frequency response of the damped and undamped LCL filter}
\end{figure}

**Control design: current loop**

This subsection describes the process that was followed to determine the controller gains used in the current controllers of the VSC. The VSC control block diagram is shown in Fig. 4-31 with outer voltage, \( G_v \) and inner current, \( G_i \) loop PI controllers, VSC delay, \( G_D \), VSC output inductor, \( G_L \) and LCL filter capacitor, \( G_c \). As shown, there are two PI control loops, and a feedforward loop. \( V_c \) is fed forward to the inner current loop to reduce the inner loop control action, also known as ‘back-emf decoupling’ (Loh and Holmes, 2005). The feed forward loop is also shown in the VSC control system schematic in Fig. 4-31.

In this configuration the current flowing through the inductor, \( i_L \) charges the capacitor, the aim being to maintain the output capacitor voltage, \( V_c \) as close as possible to the reference voltage provided to the voltage control loop (Rocabert et al., 2012). The outer loop provides steady-state reference tracking performance, whilst the inner loop provides fast dynamic compensation for system disturbances (Loh and Holmes, 2005), therefore the outer loop should be designed with smaller bandwidth than the inner loop (Heydari et al., 2019).
The current control loop block diagram is shown in Fig. 4-32, without the capacitor voltage feedforward loop. The structure of this loop was used to determine the inner loop transfer function. Each block in Fig. 4-32 will now be described.

The transfer function of the current loop PI controller is described by equation (4.36).

\[ G_i(s) = K_p + \frac{K_i}{s} = K_p \frac{1 + T_I s}{T_I s} \]  

where \( K_p \) and \( K_I \) are the proportional and integral gains, and \( T_I \) is the integrator time constant. There is an inherent delay associated with the PWM. This is usually one half of the sampling period, \( T_S \), and one digital processing delay (Liserre et al., 2005). Therefore the delay open loop transfer function, \( G_D(s) \) is

\[ G_D(s) = \frac{1}{1.5T_s s + 1} \]  

The sampling frequency of the current controllers is 20 kHz. At less than half of the resonant frequency of the filter, the LCL filter behaves as an L filter because the capacitor branch \( C_f \) only mitigates the high switching frequency ripple, and therefore can be assumed to be negligible. Consequently, the plant of the control loop, \( G_L(s) \) can be written as;

\[ G_L(s) = \frac{1}{L_1 s + R} \]
The current control closed loop transfer function is therefore:

$$H_i(s) = \frac{i_L}{i_{ref}} = \frac{G_i(s)G_D(s)G_L(s)}{1 + G_i(s)G_D(s)G_L(s)}$$

(4.39)

Designing the controllers for the dq axis currents on the basis of the plant time constant $T = \frac{L_1}{R}$, and setting the integrator time constant, $T_i$ equal to $T$. The closed loop transfer function becomes:

$$H_i(s) = \frac{2K_p/3L_1T_s}{s^2 + \frac{2s}{3T_s} + \frac{2K_p}{3L_1T_s}}$$

(4.40)

From the well-known form of

$$H_i(s) = \frac{\omega_n^2}{s^2 + 2\zeta \omega_n s + \omega_n^2}$$

(4.41)

where $\zeta$ is the damping ratio and $\omega_n$ is the undamped natural frequency. Therefore, comparing (4.40) and (4.41) then:

$$\omega_n^2 = \frac{2K_p}{3L_1T_s}$$

(4.42)

And

$$\zeta \omega_n = \frac{1}{3T_s}$$

(4.43)

The damping ratio for VSC controllers is typically 0.707 (Teodorescu et al., 2011; Yazdani, 2008). Therefore rearranging (4.42) and (4.43), it can be shown that the proportional constant of the PI controller can be calculated from

$$K_p = \frac{L_1}{3T_s} = 0.68$$

(4.44)

From (4.36), the integral constant of the PI controller is given by (4.45).
\[ K_I = \frac{K_p}{T_i} = \frac{K_p}{T} = 4.10 \]

The bandwidth frequency of the inner control loop is given by (4.46) (Teodorescu et al., 2011)

\[ f_{bi} = \frac{1}{6\pi T_s} \]

A stability assessment using a root locus plot of the closed loop system response in the z domain is shown in Fig. 4-33. The plot shows that the poles and zeros of the system are within the unit circle, therefore the system is stable. The desired damping is achieved as shown from the location of the poles, the grid in the unit circle of Fig. 4-33 shows the damping factors from 0 to 1, the poles of the system are shown to be at a damping factor of 0.707. The step response of the inner loop is shown in Fig. 4-34, showing a 4.3% overshoot. The final current control loop design parameters are provided in Table 4-6.

Fig. 4-33. Root locus plot of the poles (x) and zeroes (o) for the closed loop design of the VSC current controller
Control design: voltage loop

The voltage control loop block diagram is shown in Fig. 4-35, showing the voltage loop PI controller, inner current loop, and LCL filter capacitor. The voltage loop should be designed to be an order of magnitude slower than the inner loop to avoid mutual coupling of the control loops (Jarwar et al., 2018). If the control loops were of similar bandwidth, there would be continuous adjustments of the reference currents provided from the outer voltage loop to the inner current loop, which would result in persistent oscillations in the output response of the control system. The outer loop bandwidth was designed here to be 1/10th that of the inner loop bandwidth. At a damping ratio of 0.707, the natural frequency is approximately equivalent to the controller bandwidth. This is described mathematically by:

$$\omega_{bw} = \omega_n \sqrt{1 - 2\xi^2 + (2 - 4\xi^2 + 4\xi^4)^{0.5}}$$  \hspace{1cm} (4.47)
The voltage loop PI controller, $G_v(s)$, takes the same form of equation (4.36). The capacitor transfer function is given by

$$G_C(s) = \frac{1}{C_f s}$$

The current controller was designed to be optimally damped, therefore the current controller closed loop transfer function can be approximated by a first order system when calculating the bandwidth of the system (Teodorescu et al., 2011), given by (4.49).

$$H_1(s) \approx \frac{1}{3T_c s + 1}$$

The closed loop transfer function of the voltage loop controller is:

$$H_v(s) = \frac{v_c}{v^*} = \frac{G_v(s)H_1(s)G_C(s)}{1 + G_v(s)H_1(s)G_C(s)}$$

Which becomes

$$H_v(s) = \frac{K_{pv}s + K_{iv}}{3C_f T_s s^3 + C_f s^2 + K_{pv}s + K_{iv}}$$

For the purposes of calculating the controller gains the integral gain is first set to $K_i= 0$, therefore $H_v$ becomes

$$H_v(s) = \frac{K_{pv}/3C_f T_s}{s^2 + \frac{s}{3T_c} + \frac{K_{pv}}{3C_f T_s}}$$

Therefore from (4.41) the third term of the denominator in (4.52) can be determined using:

$$\omega_n^2 = \frac{K_{pv}}{3C_f T_s}$$

As discussed above, the bandwidth of the voltage loop controller is $1/10^{th}$ that of the current controller, and if the system is optimally damped, $\omega_n \approx \omega_{bus}$. Therefore

$$K_{pv} = 0.403$$

The step response of the closed loop transfer function described by (4.51) is shown in Fig. 4-36 for $K_i$.
between 1 and 15. The $K_i$ value of 5 was selected to provide system damping whilst limiting overshoot to below 5%. The parameters of the outer loop controller are provided in Table 4-7.

![Fig. 4-36. VSC voltage control loop unit step response used to determine Ki of the PI controllers](image)

### Table 4-7. Outer voltage loop controller parameters for the VSC

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
<th>Unit</th>
</tr>
</thead>
<tbody>
<tr>
<td>Sampling frequency, $f_s$</td>
<td>5</td>
<td>kHz</td>
</tr>
<tr>
<td>Proportional gain, $K_p$</td>
<td>0.403</td>
<td></td>
</tr>
<tr>
<td>Integral gain, $K_i$</td>
<td>5</td>
<td></td>
</tr>
<tr>
<td>Current controller bandwidth, $f_{hi}$</td>
<td>100</td>
<td>Hz</td>
</tr>
</tbody>
</table>

#### 4.7.2 Steady state efficiency model of VSC converter and LCL filter

The two-level converter efficiency model for modelling tool 1 is implemented using an averaged power loss model. This model comprises the average IGBT conduction and switching loss, and antiparallel diode conduction and reverse recovery loss over a switching cycle. The method used to calculate the losses for two-level VSCs is detailed in Sharkh et al. (2014) and is used here to calculate the VSC efficiency curve. The conduction losses for each IGBT and antiparallel diode in the VSC are given by (4.54) and (4.55) respectively.

$$P_{con,IGBT} = \frac{V_{CE0}I_c}{2\pi} + \frac{r_cI_c^2}{8} + m_a\cos\phi \left(\frac{V_{CE0}I_c}{8} + \frac{r_cI_c^2}{3\pi}\right)$$  \hspace{1cm} (4.54)

$$P_{con,diode} = \frac{V_{d0}I_c}{2\pi} + \frac{r_dI_c^2}{8} - m_a\cos\phi \left(\frac{V_{d0}I_c}{8} + \frac{r_dI_c^2}{3\pi}\right)$$  \hspace{1cm} (4.55)

where $P_{con,IGBT}$ and $P_{con,diode}$ are the conduction losses of an IGBT and diode respectively. $I_c$ is the DC collector current, $V_{CE0}$ is the IGBT zero collector emitter voltage, $V_{d0}$ is the diode on-state voltage, $r_c$ and
$r_d$ are the on-state resistances of the devices derived from the manufacturer’s data sheet. $m_a$ is the modulation index and $\cos \phi$ is the power factor of the load. The characteristics used to calculate the conduction losses of the devices in the VSC in the steady state model are provided in Table 4-8.

Table 4-8. Characteristics used to calculate the conduction losses of the IGBT and diode devices (ABB, 2014)

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
<th>Unit</th>
</tr>
</thead>
<tbody>
<tr>
<td>IGBT zero collector emitter voltage $V_{CE0}$</td>
<td>1.15</td>
<td>V</td>
</tr>
<tr>
<td>IGBT on state resistance $r_c$</td>
<td>0.56</td>
<td>mΩ</td>
</tr>
<tr>
<td>Diode on state voltage $V_{do}$</td>
<td>1.95</td>
<td>V</td>
</tr>
<tr>
<td>Diode on state resistance $r_d$</td>
<td>0.24</td>
<td>mΩ</td>
</tr>
<tr>
<td>Assumed modulation index, $m_a$</td>
<td>0.8</td>
<td></td>
</tr>
<tr>
<td>Assumed power factor, $\cos \phi$</td>
<td>0.8</td>
<td></td>
</tr>
</tbody>
</table>

The switching losses of the IGBT and diode are described by (4.56) and (4.57) respectively. The turn on and turn off energy loss is captured for the IGBT and only the recovery loss is considered for the diode. The turn on energy of the diode is disregarded (Sharkh et al., 2014). The polynomial expressions for the switching energies of the power electronic devices are derived from the manufacturer’s data sheet (ABB, 2014) and are provided in Appendix I.

\[ P_{sw,IGBT} = f_{sw}(E_{on} + E_{off}) \left( \frac{V_{bess}}{V_{base}} \right) \]  
(4.56)

\[ P_{rec,diode} = f_{sw}(E_{rec}) \left( \frac{V_{bess}}{V_{base}} \right) \]  
(4.57)

where $P_{sw,IGBT}$ and $P_{rec,diode}$ are the switching and recovery losses of an IGBT and diode respectively. $E_{on}$ and $E_{off}$ are the turn-on and turn-off switching energies of the IGBT. $E_{rec}$ is the recovery energy of the diode. $V_{base}$ is the base voltage used by the manufacturer to determine the switching energy of the devices and is used to scale the losses to the battery voltage. The total losses in the converter are therefore given by (4.58). There are 18 devices in the VSC, as there are three switches per IGBT module, and two modules per phase leg of the VSC.

\[ P_{loss,VSC} = 18 \times (P_{con,IGBT} + P_{con,diode} + P_{rec,diode} + P_{sw,IGBT}) \]  
(4.58)

The resulting efficiency curve of the VSC up to rated power when supplying a 0.8 PF lagging load is shown in Fig. 4-37. The LCL filter is assumed to have a 1% loss (Beres et al., 2016), this is not shown in the VSC efficiency plot in Fig. 4-37.


4.8 Transformer model

4.8.1 Dynamic model

The model of the transformer located between the LCL filter and the main switchboard in modelling tool 2 uses the SimPowerSystems blockset model of a three phase linear transformer. The per phase equivalent circuit is shown in Fig. 4-38. This model is a simplification of a three phase transformer with three single phase transformers, a full description of which is offered by The MathWorks, Inc (2018d). The transformer is connected in a star-delta (YΔ1) configuration. As discussed in chapter 3, this is a robust method of providing galvanic isolation and prevents common mode currents from propagating from the VSC to the main distribution system. The transformer model takes into consideration the primary and secondary winding resistances and inductive reactance. The magnetizing characteristics of the core are modelled as a linear impedance. The rating of the transformer was selected such that it would be able to transmit the full discharge power of the ESS when the converter is operating at 0.8 PF lagging. The parameters of the transformer model are provided in Table 4-9. These are representative of equivalently rated transformers as described by Wildi (2014).

![Transformer per phase equivalent circuit](image)

Fig. 4-38. Transformer per phase equivalent circuit.
Table 4-9. Transformer model parameters

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
<th>Unit</th>
</tr>
</thead>
<tbody>
<tr>
<td>Transformer rating, $P_n$</td>
<td>3</td>
<td>MW</td>
</tr>
<tr>
<td>Transformer primary voltage, $V_1$ / secondary voltage $V_2$</td>
<td>480/690</td>
<td>V</td>
</tr>
<tr>
<td>Primary winding resistance, $R_{11}$</td>
<td>0.002</td>
<td>pu</td>
</tr>
<tr>
<td>Primary winding inductance, $L_{11}$</td>
<td>0.05</td>
<td>pu</td>
</tr>
<tr>
<td>Secondary winding resistance, $R_{21}$</td>
<td>0.002</td>
<td>pu</td>
</tr>
<tr>
<td>Secondary winding inductance, $L_{21}$</td>
<td>0.05</td>
<td>pu</td>
</tr>
<tr>
<td>Magnetization resistance, $R_m$</td>
<td>500</td>
<td>pu</td>
</tr>
<tr>
<td>Magnetization inductance, $L_m$</td>
<td>500</td>
<td>pu</td>
</tr>
</tbody>
</table>

4.8.2 Steady state efficiency model

The transformer efficiency model, used in modelling tool 1, applies the core and copper loss characteristics based upon the parameters provided in Table 4-9. The method to derive the transformer losses is provided in Appendix J. The transformer efficiency plot is provided in Fig. 4-39 assuming a load at PF of 0.8 lagging. The efficiency plot exhibits a relatively linear relationship from above 20% of rated power tending to 99% efficiency, which is typical transformer efficiency at rated load (Targosz et al., 2012). The polynomial function of the transformer efficiency characteristic in Fig. 4-39 is estimated using (4.59) and included in the steady state efficiency model.

\[
n_{tx} = \frac{0.045P_{tx}^3 + (8.9 \times 10^5)P_{tx}^2 - (1.6 \times 10^5)P_{tx} - 0.0016}{P_{tx}^3 + 8981P_{tx}^2 + 648P_{tx} - 400}
\]  

(4.59)

where $P_{tx}$ is the output power of the transformer as a percentage of maximum power.

4.9 Diesel generator model

The DG model is representative of a high speed DG of the type that would be installed for power generation in naval warships. First, the dynamic model of the DG that is used in modelling tool 2 will be described. The actual dynamic Simulink model schematic of the diesel engine, synchronous machine and AVR can be
found in Appendix K. The SFC and efficiency model of the DG used in modelling tool 1 is presented after the dynamic model.

4.9.1 Dynamic model

The diesel engine model used in this research utilises the SimPowerSystems blockset diesel engine model, a full description of which is given by Yeager and Willis (1993). The diesel engine block diagram is shown in Fig. 4-40, highlighting the transfer function representation of the governor controller and actuator, and the engine time delay.

The parameters of the per unit diesel engine model are provided in Table 4-10 and are commensurate with the values used by Yeager and Willis (1993) and Mills et al. (2018). The performance of the engine model in Yeager and Willis (1993) and Mills et al. (2018) have been validated against experimental results for an emergency generator set and marine high speed diesel engine respectively. The latter within 2% of experimental results during transient conditions.

Fig. 4-40. Diesel engine model block diagram (Yeager and Willis, 1993)

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
<th>Unit</th>
</tr>
</thead>
<tbody>
<tr>
<td>Governor gain, ( K )</td>
<td>40</td>
<td></td>
</tr>
<tr>
<td>Governor controller time constants, ( T_1, T_2, T_3 )</td>
<td>0.01, 0.02, 0.2</td>
<td>s</td>
</tr>
<tr>
<td>Torque limit, ( T_{\text{max}} )</td>
<td>1.1</td>
<td>pu</td>
</tr>
<tr>
<td>Torque limit, ( T_{\text{min}} )</td>
<td>0</td>
<td>pu</td>
</tr>
<tr>
<td>Engine time delay, ( T_d )</td>
<td>0.024</td>
<td>s</td>
</tr>
<tr>
<td>Actuator time constants, ( T_4, T_5, T_6 )</td>
<td>0.25, 0.009, 0.0384</td>
<td>s</td>
</tr>
</tbody>
</table>

Synchronous machine model

The generator was modelled using a \( dq \) representation of a synchronous machine from the SimPowerSystems blockset. A description of the model is provided by The MathWorks Inc, (2018b) and further reading on \( dq \) axis synchronous machine models is offered by Kundur (1994a). The model implements both the machine’s electrical and mechanical characteristics. The equivalent circuit of the synchronous machine model is shown in Fig. 4-41 as viewed from the stator. The \( dq \) axis model was selected as it captures the transient characteristics of the generator as the dynamics of the stator, field and amortisseur windings are modelled. This is important to accurately capture the electrical performance of
the system in modelling tool 2, to ensure that the behaviour of the generator and VSC are accurately captured during load sharing simulations.

\[ \Delta \omega_r(t) = \frac{1}{2H} \int_0^t (T_m - T_e) dt - K_d \Delta \omega_r(t) \]  

(4.60)

The mechanical model implements the mechanical system as detailed by (4.60) and (4.61). Equation (4.60) calculates the rotor speed variation. Equation (4.61) then computes the mechanical speed of the rotor by summing the speed variation and the rated reference speed of the machine. In determining the rotor speed variation, the inertia constant, accelerating torque required to overcome the applied load and damping factor due to the amortisseur windings are taken into account.

The notation of the equivalent circuit in Fig. 4-41 is as follows:

- \( R_s \) - Stator resistance
- \( L_l \) - Stator inductance
- \( \omega_r \) - Rotor angular velocity
- \( \psi \) - Stator flux linkage (d and q)
- \( L_a \) - Flux linkage between field and amortisseur winding (d and q)
- \( R_1 \) - Amortisseur winding 1 resistance (d and q)
- \( R_2 \) - Amortisseur winding 2 resistance (q axis only)
- \( i_{1d} \) - Amortisseur winding 1 current (d and q axis)
- \( i_{2q} \) - Amortisseur winding 2 current
- \( i_{fd} \) - Synchronous machine field winding current
- \( L_{fd} \) - Synchronous machine field winding inductance
- \( e_{fd} \) - Synchronous machine field winding voltage
- \( V_{d} \) - Stator voltage direct axis component
- \( V_{q} \) - Stator voltage quadrature axis component

Fig. 4-41. Synchronous generator equivalent circuit (The MathWorks Inc, 2018d)
\[ \omega_r(t) = \Delta \omega_r(t) + \omega_0 \quad (4.61) \]

where \( \Delta \omega_r \) is the speed variation with respect to speed of operation in (rad/s), \( H \) is the machine constant of inertia (s), \( T_m \) and \( T_e \) are the mechanical and electromagnetic torque (Nm). \( K_d \) is the damping factor representing the effect of the amortisseur windings (pu). \( \omega_r(t) \) is the mechanical speed of the rotor (rad/s) and \( \omega_0 \) is the rated speed of operation (rad/s). The mechanical block diagram of the synchronous machine is shown in Fig. 4-42.

The synchronous machine parameters have been taken from a datasheet provided by Cummins Generator Technologies (AvK-Alternators, 2014) which details a high speed synchronous machine rated at 2.83 MW, 690 V 60 Hz. The key parameters of the datasheet are provided in Table 4-11. The real power rating of the synchronous machine is 170 kW less per machine than the candidate warship power system described in chapter 3. However, this was considered an acceptable compromise for modelling tool 2 for three reasons. First, the power deficit is countered by the battery ESS. If the switchboards are operating in split bus configuration, each battery ESS can supply the combined 340 kW deficit in power demand for one hour if required (assuming the ESS is at 90% SoC before demand). Second, the parameters selected are commensurate with a high speed machine that could be installed in a warship power generation plant. Third, the detail provided in the datasheet provides a high level of verification of the synchronous machine model against the manufacturer’s data. Verification is detailed in section 4.13.
Table 4-11. Synchronous machine parameters (AvK-Alternators, 2014)

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
<th>Unit</th>
<th>Parameter</th>
<th>Value</th>
<th>Unit</th>
</tr>
</thead>
<tbody>
<tr>
<td>Real power $P_n$</td>
<td>2831</td>
<td>kW</td>
<td>Quadrature axis synchronous reactance $X'_{q}$</td>
<td>1.2</td>
<td>pu</td>
</tr>
<tr>
<td>Line voltage $V_{rms}$</td>
<td>690</td>
<td>V</td>
<td>Quadrature axis transient reactance $X'_{q}$</td>
<td>1.2</td>
<td>pu</td>
</tr>
<tr>
<td>Power factor PF</td>
<td>0.8</td>
<td></td>
<td>Quadrature axis sub transient reactance $X_{q}'$</td>
<td>0.185</td>
<td>pu</td>
</tr>
<tr>
<td>Frequency $F$</td>
<td>60</td>
<td>Hz</td>
<td>Leakage reactance $X_l$</td>
<td>0.055</td>
<td>pu</td>
</tr>
<tr>
<td>Pole pairs $p$</td>
<td>4</td>
<td></td>
<td>Transient short circuit time constant $T'_{d}$</td>
<td>0.44</td>
<td>s</td>
</tr>
<tr>
<td>Stator Resistance $R_s$</td>
<td>0.002</td>
<td>pu</td>
<td>Sub transient short circuit time constant $T'_{q}$</td>
<td>0.021</td>
<td>s</td>
</tr>
<tr>
<td>Direct axis synchronous reactance $X_d$</td>
<td>2.40</td>
<td>pu</td>
<td>Quadrature axis transient short circuit constant, $T'_q$</td>
<td>0.42</td>
<td>s</td>
</tr>
<tr>
<td>Direct axis transient reactance $X'_{d}$</td>
<td>0.305</td>
<td>pu</td>
<td>Quadrature axis sub-transient short circuit time constant $T'_{d}$</td>
<td>0.042</td>
<td>s</td>
</tr>
<tr>
<td>Direct axis sub transient reactance $X'_{d}$</td>
<td>0.185</td>
<td>pu</td>
<td>Inertia Constant $H$</td>
<td>2.28</td>
<td>s</td>
</tr>
</tbody>
</table>

AVR model

The AVR model employed in this research utilises the SimPowerSystems blockset model of an IEEE Type-2 AVR static excitation system, a detailed description for which is provided by Kundur (1994a) and The MathWorks Inc (2018c). The AVR model is a compound source rectifier excitation system that controls the generator terminal voltage and reactive power loading by controlling the DC field current applied to the machine field windings. The AVR controls the exciter output through controlled saturation of the power transformer. When the generator is not supplying load the armature current is zero. Under load conditions, part of the power is derived from the generator output current.

A schematic of the model is provided in Fig. 4-43. The actual Simulink schematic is provided in Appendix K. The parameters of the AVR used in this research are provided in Table 4-12, these were selected from the parameters employed by Tsekouras et al. (2010) for a 2 MVA synchronous machine representative of diesel generator sets used in low voltage marine power systems.

![Fig. 4-43. IEEE type ST2A excitation system model schematic (The MathWorks Inc, 2018e)](image-url)
Table 4-12. Type-2 AVR parameters employed as selected from Tsekouras et al. (2010)

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
<th>Unit</th>
<th>Parameter</th>
<th>Value</th>
<th>Unit</th>
</tr>
</thead>
<tbody>
<tr>
<td>Low pass filter time constant $T_R$</td>
<td>0.02</td>
<td>s</td>
<td>Damping filter time constant $T_f$</td>
<td>0.10</td>
<td>s</td>
</tr>
<tr>
<td>Voltage regulator gain $K_a$</td>
<td>200</td>
<td></td>
<td>Exciter gain $K_e$</td>
<td>1.00</td>
<td></td>
</tr>
<tr>
<td>Voltage regulator time constant $T_a$</td>
<td>0.001</td>
<td>s</td>
<td>Exciter time constant $T_E$</td>
<td>0.80</td>
<td>s</td>
</tr>
<tr>
<td>Regulator output limits, $V_{min}, V_{max}$</td>
<td>-6, 6</td>
<td>V</td>
<td>Potential circuit gain coefficient $K_p$</td>
<td>5.00</td>
<td></td>
</tr>
<tr>
<td>Damping filter gain $K_f$</td>
<td>0.001</td>
<td></td>
<td>Rectifier loading factor $K_c$</td>
<td>1.82</td>
<td>pu</td>
</tr>
</tbody>
</table>

4.9.2 Steady state fuel consumption and efficiency model

The diesel engine model SFC characteristic was derived from reference data in Allen and Buckingham (2017) for a high speed diesel engine operating at a synchronous speed of 1800 rpm as a percentage of rated engine power. The reference SFC data was then fitted to a polynomial function as shown in Fig. 4-44(a) and described by (4.62). The SFC characteristic of the engine is comparatively high at low load, particularly when below 50% rated power as seen in Fig. 4-44(a). The normalised root mean squared error (NRMSE) over the power range of the engine against the reference data is 2.6%. The generator efficiency curve was derived from the manufacturer’s datasheet (AvK-Alternators, 2014) and is plotted in Fig. 4-44(b). The polynomial function is provided in (4.63). The generator efficiency model assumes a 0.8 PF lagging load. A power factor of 0.8 was selected to explore a worst case normal operating condition. It is acknowledged that diesel generators rated to 0.85 or 0.9 PF would increase the efficiency of the generator as shown in Fig. 4-44(b) and could reduce operating cost for a small warship. Here the generator efficiency is above 92% over its entire load range and it is most efficient when operating above 50% rated load.

\[
SFC_{DE} = (2.354 \times 10^{-5})P_{DE}^2 - 0.1298 P_{DE} + 386.8 \quad (4.62)
\]

\[
n_{gen} = (-1.07 \times 10^{-7})P_g^4 + (3.9 \times 10^{-5})P_g^3 - 0.006P_g^2 + 0.38P_g + 87 \quad (4.63)
\]

where $P_{DE}$ is the engine mechanical power (kW), and $P_g$ is the percentage electrical real power of 3 MWc.
4.10 Transmission line model

The cable model is designed to be a short transmission line as it is less than 80 km in length. The shunt capacitance to earth is considered negligible and the transmission model considers the series impedance comprising \( R_c \) and \( L_c \) of the cables only (Kundur, 1994d). As the generator is star connected, the line current is equal to the phase current, thus the line current is

\[
I_L = \frac{S}{V\sqrt{3}} = \frac{3.51 \times 10^6}{690 \times \sqrt{3}} = 2937 \text{ A}
\]  

(4.64)

With 5 conductors per phase, the required current carrying capacity of each conductor is 588 A. With a margin for de-rating, the parameters of each cable are given in Table 4-13 and are based on the parameters provided by Nexans AmerCable (2018). The cables between the generator and switchboard are considered to be 10 m in length. The ESS transformer is considered to be co-located with the switchboard and therefore the transmission distance is assumed negligible.

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Size AWG/kcmil</td>
<td>535</td>
</tr>
<tr>
<td>Cross section area</td>
<td>273 cm²</td>
</tr>
<tr>
<td>AC resistance</td>
<td>1.08 mΩ/10 m</td>
</tr>
<tr>
<td>Inductive reactance</td>
<td>1.02 mΩ/10 m</td>
</tr>
<tr>
<td>Current carrying capacity</td>
<td>682 A</td>
</tr>
</tbody>
</table>

4.11 Power sharing operation

This section describes the components that permit power sharing of the DG and ESS model used in modelling tool 2. The main features of the power management architecture described in this section are
highlighted by the red dashed boxes in Fig. 4-45. First, the synchroniser circuit is described that synchronises the VSC output with the main switchboard AC voltage. Second, the droop control implementation is detailed and the droop coefficients selected. The power management strategy implemented in the power management block of the model is described in chapter 5.

![Diagram](image)

**Fig. 4-45. Modelling tool 2 schematic highlighting the droop control layer and synchroniser**

### 4.11.1 Synchroniser circuit

A synchronisation circuit was implemented in the model to control the breaker that connects the output of the transformer to the main switchboard. The aim of the synchroniser is to control offsets to the voltage and frequency set points in the secondary control layer of the battery ESS until the output voltage waveform at the output of the transformer is synchronised with the grid’s voltage magnitude, frequency and phase. The offset signals are controlled by independent acting PI controllers. The block diagram of the synchroniser is shown in Fig. 4-46. The actual Simulink model schematic is provided in Appendix O.

The synchronisation criteria to enable the breaker to close are such that the voltage magnitude, frequency and phase difference are less than 5 V, 0.5 Hz and 0.2° respectively. If these criteria are met, there is a 2 s delay before the breaker is closed to ensure the system has reached steady state in terms of voltage, frequency and phase. This logic is included in the breaker logic block in Fig. 4-46. When the switch is closed, offsets cannot be added to the VSC main control system. The phase controller, \( PI_\phi \), is slower than the frequency PI controller, \( PI_f \), and is only permitted to add frequency offsets when the frequency error is less than 1 Hz. This prevents the controllers from conflicting with each other.
4.11.2 Droop control

In isolated microgrids, power systems that are either VSC fed or DG fed, normally do not require communication between sources when droop control is implemented (Al-Falali et al., 2018; Rocabert et al., 2012). This also applies to the candidate ship power system in this research. The droop control introduces droop in terminal voltage and frequency amplitude of the converter and DG, which prevents circulating currents being transmitted between the generator and converter. The voltage and frequency droop control loops for the converter and DG control systems are shown in Fig. 4-47 and Fig. 4-48 respectively, implementing the droop control equations (2.1) and (2.2) described in section 2.3.2. The Simulink implementation of droop control is provided in Appendix G and Appendix K for the VSC and DG respectively.

The droop coefficients were initially selected based on the deviation limits permitted by ship QPS standards for AC systems. This is recognised practice as discussed by Vasquez et al. (2013), Guerrero et al., (2013) and Heydari et al. (2019). The droop coefficients are therefore given by (4.65) and (4.66).
\[ m = \frac{\Delta f}{P_{\text{max}}} \]  \hspace{1cm} (4.65)

\[ n = \frac{\Delta V}{Q_{\text{max}}} \]  \hspace{1cm} (4.66)

where \( \Delta f \) and \( \Delta V \) are the maximum frequency and voltage deviations allowed, and \( P_{\text{max}} \) and \( Q_{\text{max}} \) are the maximum active and reactive powers delivered by the converter or DG respectively. Note that for the ESS, \( P_{\text{max}} \) and \( Q_{\text{max}} \) cover the charge and discharge range as shown in Fig. 2-12 in chapter 2. As discussed in chapter 3, for warships of NATO navies, NATO STANAG 1008 is recognised as the baseline standard for shipboard LV systems with regard to QPS criteria (Mills et al., 2018; Whitelegg, 2016b). Under steady state conditions, STANAG 1008 specifies a maximum permanent variation of \( \pm 3\% \) and \( \pm 5\% \) for frequency and voltage respectively. Consequently, the droop coefficients are provided in Table 4-14. \( n_{\text{VSC}} \) is calculated referred to the rating on the switchboard side of the transformer. The droop curves for the VSC and DG are plotted in Fig. 4-49(a) and (b). The VSC voltage droop is referred to the main switchboard voltage in Fig. 4-49 (b). The droop curves have similar gradients for frequency. Conversely due to the lower reactive power rating of the converter the droop coefficient is of greater magnitude.

**Table 4-14. Frequency and voltage deviation limits with corresponding droop coefficients**

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
<th>Unit</th>
</tr>
</thead>
<tbody>
<tr>
<td>Maximum frequency deviation</td>
<td>± 3%</td>
<td></td>
</tr>
<tr>
<td>DG frequency droop, ( m_{\text{DG}} )</td>
<td>0.64</td>
<td>Hz/MW</td>
</tr>
<tr>
<td>VSC frequency droop, ( m_{\text{VSC}} )</td>
<td>0.60</td>
<td>Hz/MW</td>
</tr>
<tr>
<td>Maximum voltage deviation</td>
<td>± 5%</td>
<td></td>
</tr>
<tr>
<td>DG voltage droop, ( n_{\text{DG}} )</td>
<td>16.27</td>
<td>V/MVAr</td>
</tr>
<tr>
<td>VSC voltage droop, ( n_{\text{VSC}} )</td>
<td>18.55</td>
<td>V/MVAr</td>
</tr>
</tbody>
</table>

![Diagram](image-url)

Fig. 4-49. (a) Frequency and (b) voltage droop characteristics of the DG and VSC. Note that the VSC droop has been referred to the main switchboard voltage.

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4.12 DC/DC boost converter model and control system design

The DC/DC boost converter model is comprised of two parts. The single stage boost converter, with PWM pulse generator, and the boost converter control system. Justification for using the single stage boost converter was provided in section 3.6. The specification of the components in the boost converter will be detailed first followed by the control system.

4.12.1 Component specification

The single stage DC/DC boost converter circuit as shown in Fig. 4-50, implements the SimPowerSystem models of the inductor, IGBT and diode switches, and capacitor. The pulse generator for the IGBT module is implemented using the PWM DC-DC generator block from the SimPowerSystems library, a full description of which is given by The MathWorks, Inc (2018a). The actual model schematic of the converter and controller used in the model is provided in Appendix L. The pulse generator uses the duty cycle, \( D \) which is obtained from the boost converter control system to define when the IGBT switching module conducts. Theory of boost converter concept of operation is comprehensively detailed by Choi (2013) and Kazimierczuk, (2016). The concept of operation of the boost converter is described in section 3.6.

\[
\begin{align*}
    i_{\text{bess}} & \quad L \quad i_L \quad D_1 \quad i_o \\
    + & \quad - \\
    v_{\text{bess}} & \quad \text{Duty cycle, } D \\
    \text{SW} & \quad C \quad \text{+} \\
    v_o & \quad -
\end{align*}
\]

Fig. 4-50. DC/DC boost converter circuit

To manage the high current associated with the peak pulse power demand of the LDEW, the IGBT and diode characteristics used in the boost converter are based on the ABB 5SNA and 5SLA modules respectively (ABB 2014, 2015). The device’s 1.7 kV, 3.6 kA collector current rating and 8.25 MA/s rate limit are compatible with the LDEW pulse rise time and peak power. These ratings satisfy the 3.07 kA maximum discharge current of the battery ESS. The switching frequency of the power electronic devices was set at 5 kHz. This is equivalent to the switching frequency of IGBT devices used in high power DC/DC converters used in parallel sector applications, examples of which can be found in Deng and Chen (2013) and Agamy et al, (2017).

The output capacitor is specified to satisfy the discharge current of the LDEW load (1.37 kA) whilst minimising the output voltage ripple. The relationship between the capacitor rating and voltage ripple is detailed by (4.67) (Czarkowski, 2007).

\[
C = \frac{D_{\text{nom}}v_o}{\Delta v f_s R_{\text{load, max}}} \quad (4.67)
\]
where \( \Delta v_c \) is the voltage ripple and \( R_{load,max} \) is the maximum peak power of the LDEW load. The duty cycle range of the converter, assuming 90% efficiency of the converter (Kazimierczuk, 2016), can be estimated using:

\[
D_{\text{max}} = 1 - \frac{\eta v_{\text{bess min}}}{v_o}
\]

\[
D_{\text{nom}} = 1 - \frac{\eta v_{\text{bess nom}}}{v_o}
\]

\[
D_{\text{min}} = 1 - \frac{\eta v_{\text{bess max}}}{v_o}
\]

The voltage ripple should be maintained below 5%, and the tolerance limits of the output voltage are ±10% as recommended by current practice (IEEE Industry Applications Society, 2018). Therefore, the capacitor needs to have a minimum rating of 1,650 V. The parameters of the DC link capacitor used in the simulation model at the output of the DC/DC converter are based on the E50.U65-764F51 capacitor offered by Electronicon (2017). This particular capacitor is rated at 1,800 V, 200A, which meets the voltage requirement with enough margin for de-rating. Seven capacitors are required in parallel to manage the peak output current, \( i_o \) of 1.37 kA. The corresponding voltage ripple was calculated using (4.67) to be 1.4%, which is within the mandated 5% tolerance.

The capacitor model in the simulation model is a simplification of the equivalent circuit of a capacitor with equivalent series resistance (ESR). The equivalent leakage resistance has not been taken into account. Modelling of the leakage resistance was not considered necessary as this research is not concerned with the ability of the DC link capacitor to store energy for a significant duration. The capacitor parameters are summarised in Table 4-15.

<table>
<thead>
<tr>
<th>Reference data</th>
<th>Value</th>
<th>Model parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Module capacitance</td>
<td>757 ( \mu )F</td>
<td>Capacitor rating</td>
<td>5.3 mF</td>
</tr>
<tr>
<td>Module series resistance</td>
<td>0.38 m( \Omega )</td>
<td>Capacitor series resistance</td>
<td>0.054 m( \Omega )</td>
</tr>
<tr>
<td>Maximum module rms current</td>
<td>200 A</td>
<td>Rated current</td>
<td>1.4 kA</td>
</tr>
<tr>
<td>Modules required in parallel</td>
<td>7</td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

The inductor model is represented by its inductance and ESR. There are a number of trade-offs that need to be considered when specifying these parameters. It is important to minimise the current stress on the battery ESS and the switching devices that can arise from current ripple, whilst minimising size and the losses of the inductor. The latter is important as increased losses will not only generate waste heat that needs to be managed by the ships cooling system, but the scale of the losses will influence the power draw from the battery ESS to meet the LDEW load demand, which needs to be kept within the discharge limits of the battery. It is therefore desirable to minimise the resistance to ensure that the battery ESS has the capacity to meet the LDEW load demand over its range of SoC. This is because the voltage drop will be minimised per shot (activation) of the LDEW. Therefore, with lower resistance, more shots are available.
Modelling

from the battery ESS before the cut-off voltage is reached and the battery can no longer supply the LDEW. The process used to determine the inductor inductance and ESR is as follows:

1) The switching operation of the boost converter was described previously in section 3.6.1, where the inductor current was shown to vary with the switching frequency of the IGBT power electronic devices. Considering Fig. 4-51, which describes how the inductor current increases over time during a ramp event. At the instance when there is a rapid change in required power, the battery needs to discharge to meet the demand, at such time the average duty cycle, $D$ can be assumed to tend to 1 (Barati et al., 2013), consequently the average current output and therefore slew rate of the inductor, $\frac{dI_L}{dt}$ can be calculated using (4.68) and (4.69) respectively.

![Fig. 4-51. DC/DC boost converter inductor current during a ramp event](image)

$$\frac{dI_L}{dt} = \frac{V_o - V_L}{2L}$$  \hspace{1cm} (4.68)

$$I_L = DT_s (V_o - V_L)/L$$

$$I_L = T_s (1-D)V_L/L$$  \hspace{1cm} (4.69)

As discussed in chapter 3, Titterton (2015) stated that the time to full radiant intensity of an SSL is in the order of 10’s of milliseconds. The fastest LDEW time to full brightness selected for this work is 25 ms, therefore the input inductor inductance of the DC/DC converter must ensure that the inductor characteristics permit $\frac{dI_L}{dt} < 25$ ms be satisfied.

2) Current ripple is a source of electromagnetic interference, therefore reducing current ripple is desirable for DC power systems. As explained in chapter 3, there is no prescribed guidance for the percentage ripple permitted. However, here the allowable peak-to-peak ripple was set at 4% of rated current at nominal voltage of the battery ESS. The battery ESS discharge current to meet the 2 MW LDEW demand assuming 90% efficiency of the boost converter (Kazimierczuk, 2016) is

$$I_{bess} = \frac{P_{LDEW}}{V_{ess,nom} \eta_{DC-DC}} = 2,267 \ A$$  \hspace{1cm} (4.70)
The inductance of the inductor operating in a DC-DC boost converter is calculated using (4.71) (Bosich et al., 2017).

\[
L = \frac{v_{\text{bess\_nom}}D_{\text{nom}}}{\Delta I_{f\text{sw}}} = 820 \, \mu H
\]  

(4.71)

Subsequently, substituting \(L=820 \, \mu H\) into (4.69) gives a slew rate limit of 317 kA/s.

3) The final step is to specify the target equivalent series resistance. The aim of this step is to minimise the losses of the inductor, which are dominated by the copper losses in the inductor winding. A limit of 2\% was specified under peak LDEW load. The required ESR was then calculated using

\[
\text{Loss} \% \times P_{\text{LDEW}} = P_{\text{cu}} = ESR \times I_L^2
\]  

(4.72)

Using the above procedure, the parameters used for the inductor in the simulation model are detailed in Table 4-16. It should be noted that these are desired parameters of the inductor for the purposes of simulation.

| Table 4-16: Selected inductor sizing parameters required for the DC/DC boost converter |
|--------------------------------------|------------------|
| Inductor model parameters           | Value            |
| Permissible current ripple           | 4\%              |
| Inductor inductance                  | 820 \( \mu \)H   |
| Inductor series resistance           | 0.0078 \( \Omega \) |

4.12.2 Control design

As determined in section 3.6.1, current mode control is the most suitable method for the single stage DC/DC boost converter. The rationale being the current drawn from the battery and output voltage of the converter are both controlled and this ensures the dynamic response of output voltage and current simultaneously (Özdemir and Erdem 2018). This mode of control has been modified in this research to account for the system dynamics of a large pulse load causing significant voltage drop across the terminals of the battery ESS. The operation of the converter controller is discussed in the following section.

**Controller requirements**

The controller requirements are as follows:

1) Control the output voltage of the converter within IEEE transient tolerance limits.

2) Meet the LDEW rise time to peak output pulse power (i.e. time to full radiant intensity).

3) To not exceed the rate of growth of current permitted by the inductor.

4) Prevent the battery discharging beyond its peak discharge current limit of 6 C.
The aim of the controller is control of the discharge of the battery by controlling the DC-DC boost converter duty cycle, $D$, the premise for which was discussed in section 3.6.1. The DC/DC boost converter utilises current mode control in a cascaded PI control structure. The control block diagram is shown in Fig. 4-52.

The slower acting outer loop regulates the output voltage $v_o$ and sets the command for the faster acting inner loop controller that is responsible for controlling the input inductor current $i_L$, subject to the maximum battery current $i_{bmax}$. The output of the inner loop controller is the duty cycle $D$, which governs the PWM switching signals that drive the IGBT switching devices. This method is called current mode control as the inductor current is controlled directly and the output voltage is controlled indirectly. PID controllers were not adopted as they would have dampened the response and increased the rise time due to its derivative term. The current mode control system in this research is supplemented with a feedforward term to mitigate against the sudden LDEW power demand and to mitigate for the position of the outer loop right hand plane zero, the position of which fluctuates with load and battery voltage, as discussed in chapter 3. Therefore, the addition of the feedforward term aims to mitigate against control system instability under the large load disturbance of the LDEW load. The feedforward current, $I_{Lff}$ uses the relation described by (4.73) to accommodate the wide input voltage variation from the battery and the large output current demand $i_o$. $I_{Lff}$ is routed to the faster acting inner control loop that controls the inductor current.

$$I_{Lff} = \frac{i_o v_o^*}{v_{bess}} \quad (4.73)$$

where $i_o$ and $v_o^*$ are the converter output current and voltage reference respectively.

Fig. 4-52. Schematic of DC/DC boost converter cascaded control configuration with feedforward loop.

The controller parameters were determined by plotting the step response of the transfer function for the closed inner loop and outer loop transfer functions of the boost converter. The proportional and integral gains of the controllers were adjusted to tune the response of the controller using the interactive tuner tool in MATLAB. A description of this tool is provided by The MathWorks (2018). Derivation of the open-loop transfer functions for current mode control of DC-DC boost converters is detailed in Kasicheyanula and John (2019) and Özdemir and Erdem (2018). The inner and outer open-loop transfer functions are provided in (4.74) and (4.75) respectively.
The PI controller transfer functions for the inner and outer loops are given by (4.76) and (4.77) respectively.

\[ G_{ID}(s) = K_P + \frac{K_i}{s} \]  \hspace{1cm} (4.76)

\[ G_{vD}(s) = K_P + \frac{K_i}{s} \]  \hspace{1cm} (4.77)

The inner loop closed-loop transfer function is

\[ H_1(s) = \frac{G_{ID}(s)G_1(s)}{1 + G_{ID}(s)G_1(s)} \]  \hspace{1cm} (4.78)

The open loop response of the outer loop is

\[ H_2(s) = G_2H_1 \]  \hspace{1cm} (4.79)

The inner loop controller gains were determined using the pidTuner tool by varying the controller gains and graphically analyzing the unit step response of the closed loop transfer function \( H_1 \). The outer loop controller gains were determined by using the open loop response of the outer control loop, \( H_2 \). The outer loop controller was designed to have 1/5th the bandwidth of the inner loop controller to prevent mutual coupling of the controllers. The outer loop’s closed loop transfer function is given by \( H_3 \)

\[ H_3(s) = \frac{G_{vD}(s)H_1(s)}{1 + G_{vD}(s)H_1(s)} \]  \hspace{1cm} (4.80)

The parameters for the two controllers are shown in Table 4-17. The parameters show that the bandwidth of the outer loop is 1/5th that of the inner loop. The corresponding unit step response for the inner and outer loops’ closed loop transfer functions is shown in Fig. 4-53. The outer loop response shows a 5% overshoot and the presence of the RHP zero as there is a phase delay in the response. This further identifies the need for a feedforward control loop. Verification of the control system design against the controller requirements
using time-domain simulation under LDEW operation is provided in section 4.13.6.

Table 4-17: DC-DC converter controller parameters

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
<th>Unit</th>
</tr>
</thead>
<tbody>
<tr>
<td>Battery output current limit, $i_{b_{\text{max}}}$</td>
<td>3.07</td>
<td>kA</td>
</tr>
<tr>
<td>Inner loop proportional gain, $K_{p1}$</td>
<td>0.0003</td>
<td></td>
</tr>
<tr>
<td>Inner loop integral gain, $K_{i1}$</td>
<td>0.079</td>
<td></td>
</tr>
<tr>
<td>Inner loop bandwidth</td>
<td>156</td>
<td>Hz</td>
</tr>
<tr>
<td>Outer loop proportional gain, $K_{p2}$</td>
<td>0.310</td>
<td></td>
</tr>
<tr>
<td>Outer loop integral gain, $K_{i2}$</td>
<td>350.4</td>
<td></td>
</tr>
<tr>
<td>Outer loop bandwidth</td>
<td>31</td>
<td>Hz</td>
</tr>
</tbody>
</table>

Fig. 4-53. Unit step response of inner loop closed loop transfer function and outer loop closed loop transfer function.

4.13 Verification and validation

This section presents the verification and validation processes conducted for the battery ESS, DG and VSC models. This was conducted to assure and confirm credibility of the results presented in chapters 5 and 6.

4.13.1 Battery model validation

The battery model was validated at cell and module level. The method used to validate the battery response was to compare the voltage response during a series of tests that the battery cell and module have undergone at Corvus Energy Ltd. For each test case, the experimental current data was inputted to the model. The voltage response of the battery was recorded and error analysis conducted. The error analysis for each test included determining the root mean square percentage error (RMSPE), maximum and mean absolute percentage error (APE) of the voltage response against experimental results. The RMSPE, maximum and mean APE are calculated using (4.81), (4.82) and (4.83) respectively.
Modelling

\[
RMSPE = 100 \sqrt{\frac{1}{n} \sum_{i=1}^{n} \left( \frac{\nu - \hat{\nu}}{\hat{\nu}} \right)^2}
\]  

(4.81)

\[
Max \ APE = \max \left| \frac{\nu - \hat{\nu}}{\hat{\nu}} \right|
\]  

(4.82)

\[
Mean \ APE = \frac{100}{n} \sum_{i=1}^{n} \left| \frac{\nu - \hat{\nu}}{\hat{\nu}} \right|
\]  

(4.83)

where \( \hat{\nu} \) are the measured values and \( \nu \) is the simulated cell or module voltage. \( n \) is the number of validation measurement instances. All measurements in the experimental results were sampled at 1 Hz.

Cell validation

The validation results of the cell simulation model against the 0.5 C pulsed discharge experimental results, upon which the model is based, is presented in Fig. 4-54(a) with the developed error of the model in Fig. 4-54(b). The experimental results in Fig. 4-54 show the battery cell being pulse discharged from 100% SoC to 0% SoC in 5% steps at 0.5 C rate. The cell simulation model response shows very good agreement with the experimental results, with 0.45% max APE between 90% and 20% SoC. The validation results outside of this range, despite showing good agreement with the experimental results, are not considered, as the cell is not permitted to operate in this range.

![Figure 4-54](image)

Fig. 4-54. (a) Validation of simulated battery cell model voltage against experimental pulsed discharge results and (b) developed error against 22°C results

A summary of the cell model response against charge and discharge tests over the range of battery operation is provided in Table 4-18. The confirmatory evidence of the validation results from Corvus Energy is
provided in Appendix A. As shown in Table 4-18, the model performs better at lower current rates. As the rate of the cell current increases, either in charge or discharge, the calculated error increases. In addition, as the rate of cell current increases the cell temperature increases. $\Delta T$ is the difference between the minimum and maximum temperature recorded during experimental testing.

Table 4-18. Battery cell validation error measurements between 90-20% SoC and recorded temperatures during experimental testing.

<table>
<thead>
<tr>
<th>Experiment</th>
<th>Max APE</th>
<th>Mean APE</th>
<th>RMSPE</th>
<th>$T_{avg}$</th>
<th>AT</th>
</tr>
</thead>
<tbody>
<tr>
<td>Pulsed discharge 0.5 C, 5% pulse rate at 22°C</td>
<td>0.45%</td>
<td>0.11%</td>
<td>0.13%</td>
<td>22°C</td>
<td>1°C</td>
</tr>
<tr>
<td>CC discharge 0.5C</td>
<td>0.81%</td>
<td>0.55%</td>
<td>0.57%</td>
<td>26°C</td>
<td>2°C</td>
</tr>
<tr>
<td>CC discharge 1C</td>
<td>1.39%</td>
<td>1.11%</td>
<td>1.15%</td>
<td>27°C</td>
<td>5°C</td>
</tr>
<tr>
<td>CC discharge 2C</td>
<td>2.81%</td>
<td>2.14%</td>
<td>2.23%</td>
<td>29°C</td>
<td>9°C</td>
</tr>
<tr>
<td>CC discharge 3C</td>
<td>4.44%</td>
<td>3.11%</td>
<td>3.24%</td>
<td>32°C</td>
<td>13°C</td>
</tr>
<tr>
<td>CC discharge 6C</td>
<td>8.68%</td>
<td>5.70%</td>
<td>5.90%</td>
<td>39°C</td>
<td>27°C</td>
</tr>
<tr>
<td>CCCV 0.5C</td>
<td>0.67%</td>
<td>0.37%</td>
<td>0.41%</td>
<td>26°C</td>
<td>2°C</td>
</tr>
<tr>
<td>CCCV 1C</td>
<td>1.26%</td>
<td>0.72%</td>
<td>0.85%</td>
<td>28°C</td>
<td>3°C</td>
</tr>
<tr>
<td>CCCV 2C</td>
<td>2.76%</td>
<td>1.72%</td>
<td>1.90%</td>
<td>31°C</td>
<td>8°C</td>
</tr>
<tr>
<td>CCCV 3C</td>
<td>4.19%</td>
<td>2.64%</td>
<td>2.90%</td>
<td>35°C</td>
<td>13°C</td>
</tr>
</tbody>
</table>

Cell temperature considerations

Fig. 4-55(a) presents the pulsed discharge experimental results at each tested temperature and the cell simulation results when subjected to the 0.5 C current pulses. Fig. 4-55(b) shows the percentage error between the simulated results and the experimental voltage response for the respective temperature in Fig. 4-55(a). The operating SoC limits are shown in Fig. 4-55 to highlight the error levels that pertain to the results in this research. Spikes are present in Fig. 4-55(b) for 15°C and 30°C pulsed discharge error. These spikes are a consequence of the sampling rate from the experimental data compared with the pulsed discharge experiment at 22°C, upon which the model was developed.

The key aspect to note from Fig. 4-55 is that the difference in voltage response of the experimental data for each temperature tested is small within the SoC limits of interest in this research. The difference is less than 0.2% RMS as shown in Table 4-19. The difference between the simulation model and the 22°C experimental results is 0.13% RMS. Comparatively, the simulation model results compared with the 15°C and 30°C pulsed discharge experimental results are still below 0.26% RMS difference. Because of the scale of the difference between the simulation model and experimental results in each case, it is proposed that the confidence of the results generated in this research is acceptable for temperatures from 15°C up to a maximum operating temperature of 30°C.
Modelling

Fig. 4-55. (a) Comparison of pulsed discharge experimental data at 15°C, 22°C and 30°C, and simulation model results (b) simulation model error compared to pulsed discharge experimental test data

Table 4-19. Comparison of voltage response at each temperature and against cell simulation model

<table>
<thead>
<tr>
<th>Voltage comparison</th>
<th>RMS percentage difference</th>
<th>Mean absolute percentage difference</th>
</tr>
</thead>
<tbody>
<tr>
<td>Experimental pulsed discharge 22°C vs 15°C</td>
<td>0.19%</td>
<td>0.15%</td>
</tr>
<tr>
<td>Experimental pulsed discharge 22°C vs 30°C</td>
<td>0.17%</td>
<td>0.15%</td>
</tr>
<tr>
<td>Simulation model vs Experimental pulsed discharge at 15°C</td>
<td>0.26%</td>
<td>0.22%</td>
</tr>
<tr>
<td>Simulation model vs Experimental pulsed discharge at 22°C</td>
<td>0.13%</td>
<td>0.11%</td>
</tr>
<tr>
<td>Simulation model vs Experimental pulsed discharge at 30°C</td>
<td>0.24%</td>
<td>0.22%</td>
</tr>
</tbody>
</table>

Module validation

The battery cell was scaled in 12 series 2 parallel to represent the battery module and validate against experimental results at module level. The module level validation was conducted against two separate experimental tests. The first test commences at 50% SoC with a charging stage at 1C for 45 minutes, followed by a 5 minute zero current state, a 2.5C discharge for 17 minutes follows this, before a zero current state concludes the test as shown in Fig. 4-56(b). The second test commences when the module is at 90% SoC, a constant current step is applied from 0C to 4.5C rate for a period of 5 minutes as shown in Fig. 4-56(e). The second test is representative of the discharge currents that would be exhibited during LDEW operation. Fig. 4-56(a) and Fig. 4-56(d) show the voltage response under the loading conditions for the first and second test respectively. The corresponding voltage error is shown in Fig. 4-56(c) and (f). Noise in the error signal in Fig. 4-56(c) is attributed to the measurement signals of the experimental data. The module
validation results in Fig. 4-56 show good agreement with the experimental results, the error results are provided in Table 4-20. The error results show that the maximum voltage error at module level under the discharge scenarios being tested in this research are 3.5%. The explanation for this error is twofold and are comparable to the cell level errors. First, the model parameters are based on 0.5 C pulsed discharge experimental data and then compared to the higher discharge rates used for validation purposes. Second, the cell model is isothermal. In summary, 3.5% is the maximum validation error, and is deemed low enough to consider the battery model as sufficiently validated for time-domain simulation in this research.

<table>
<thead>
<tr>
<th>Experiment</th>
<th>Max APE</th>
<th>Mean APE</th>
<th>RMSPE</th>
</tr>
</thead>
<tbody>
<tr>
<td>Module 2 C discharge</td>
<td>2.53%</td>
<td>0.23%</td>
<td>0.17%</td>
</tr>
<tr>
<td>Module 4.5 C discharge</td>
<td>3.51%</td>
<td>0.76%</td>
<td>0.72%</td>
</tr>
</tbody>
</table>

4.13.2 AVR verification

Kundur (1994c) states that the quality of electrical power in AC systems must meet pre-defined standards of constancy of voltage and frequency. In this work the response of the DG AVR was verified against the
generator control guidance in section 6.4 of Lloyd’s Register Naval Ship Rules (Lloyd’s Register, 2017). The results are summarised in Table 4-21. The results of the testing are provided in Table 4-21 and Appendix M. The results demonstrate that the AVR model satisfies the test criteria for real world generators in naval ship power systems, and therefore is deemed verified for use in this research.

Table 4-21. AVR verification results against Lloyd’s Register (2017) generator control

<table>
<thead>
<tr>
<th>Test</th>
<th>Test description</th>
<th>Criteria</th>
<th>Measurement</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>25% load</td>
<td>Voltage within ± 2.5%</td>
<td>0.01%</td>
</tr>
<tr>
<td>2</td>
<td>50% load</td>
<td>Voltage within ± 2.5%</td>
<td>0.01%</td>
</tr>
<tr>
<td>3</td>
<td>75% load</td>
<td>Voltage within ± 2.5%</td>
<td>0.01%</td>
</tr>
<tr>
<td>4</td>
<td>100% load</td>
<td>Voltage within ± 2.5%</td>
<td>0.01%</td>
</tr>
<tr>
<td>5</td>
<td>25% load – reject 25% load at 0.8 pf</td>
<td>Transient voltage rise &lt;7.5% of rated voltage</td>
<td>3.91%</td>
</tr>
<tr>
<td>6</td>
<td>50% load – reject 25% load at 0.8 pf</td>
<td>Transient voltage rise &lt;7.5% of rated voltage</td>
<td>4.03%</td>
</tr>
<tr>
<td>7</td>
<td>75% load – reject 25% load at 0.8 pf</td>
<td>Transient voltage rise &lt;7.5% of rated voltage</td>
<td>4.10%</td>
</tr>
<tr>
<td>8</td>
<td>100% load – reject 25% load at 0.8 pf</td>
<td>Transient voltage rise &lt;7.5% of rated voltage</td>
<td>4.16%</td>
</tr>
</tbody>
</table>

4.13.3 Generator model verification

The generator datasheet parameters and theoretical three phase short circuit characteristics were used as a means to determine the acceptability of the generator model for use in this research. Subsequently the key aspects used to verify the simulation against expected results were the peak fault current and sinusoidal response of the generator current to a short circuit. To verify the three-phase response one test was conducted, due to availability of results from the manufacturer data sheet. The generator model was operated at rated voltage and speed at no-load prior to application of the fault, in accordance with guidelines set out for three-phase sudden short circuit tests in IEC 66034-4 (BSI Standards, 2008). The fault was timed to occur at a zero crossing of the x-axis for phase A after the system had achieved steady state. The short circuit resistance was set at 0.01 Ω. The simulated results are provided in Fig 4-57, the peak short circuit current is 0.2% lower than the theoretical expectation of 41.5 kA, and the waveform shows good correspondence with the theoretical expectation. Evidence of this is provided in Appendix N.
4.13.4 VSC converter verification

The method used to verify the VSC controller was to compare the system response against the system set points for real power, reactive power, frequency and voltage when the battery ESS is discharging to a load via the VSC, LCL filter and transformer. The load profile is held at 0.5 MW at 0.95 PF lagging for 10 s before a ramp increase is applied. The load ramps from 0.5 MW to 2 MW in 15 s at 0.95 PF. At 35 s the load ramps down from 2 MW to 0.35 MW where it is held until 60s. The system response is shown in Fig. 4-58. Fig. 4-58(a) shows that the real power load demand is met, while the frequency response on the switchboard remains unchanged during the scenario as shown in Fig. 4-58(c). The reactive power similarly is met as shown in Fig. 4-58(b), the voltage in Fig. 4-58(d) varies during the scenario but is maintained within the steady state voltage limits of ±5%. This variation is due to the LCL filter dynamics.

Fig. 4-58. System response (a) real power (b) reactive power (c) frequency and (d) voltage response to ramp up and ramp down load profile
The total harmonic distortion (THD) was measured at the output of the transformer to ensure that it was within acceptable limits of 5% (NATO, 2004). For the scenario above in Fig. 4-58 the THD on the main distribution bus was measured, the peak THD was measured to be 0.8% at approximately 5 s. To ascertain which harmonics were present, a Fast Fourier Transform was performed over 5 cycles of the bus phase voltage to obtain the corresponding harmonic spectrum. This is shown in Fig. 4-59. Note that the fundamental has a magnitude of 100% and has been cropped to show the magnitude of the harmonics present in the switchboard voltage. The prominent harmonics present are the 5\textsuperscript{th} and 7\textsuperscript{th}, and higher frequency harmonics due to the switching frequency of the VSC.

![Harmonic Spectrum](image)

**Fig. 4-59. Main bus THD during ramp profile- 0 to 2,200 Hz**

### 4.13.5 Power sharing verification- synchroniser circuit

The synchroniser controller in modelling tool 2 (Fig. 4-45) was verified by maintaining the DG and ESS at constant load while the synchroniser matched the voltage, phase, amplitude and frequency. This enables the breaker to close to correctly engage synchronised operation. The synchronisation response is shown in Fig. 4-60. The pre-synchronisation, synchronisation and synchronised phases are highlighted in (i), (ii) and (iii) respectively. The results in Fig. 4-60 show that the synchroniser circuit performs its intended function.
**4.13.6 DC/DC converter controller verification**

The DC/DC converter controller was specifically developed to manage currents that exhibit high magnitude and short rise time to meet anticipated LDEW loads when supplied by the battery ESS. In the absence of an actual system’s test data with which to validate the control system design, the control system response was verified by applying two laser pulses at peak pulse power of 2 MW and 100 ms rise time, and assessing the power response and voltage QPS at the DC/DC converter output.

Fig. 4-61 provides the results of the control system response, both with and without the feedforward loop against the ± 10% voltage tolerance specified by IEEE Industry Applications Society (2018). Comparison of Fig. 4-61(a) and Fig. 4-61(c) shows that when the feedforward loop is absent, despite providing the pulse power required, the output voltage dips to 24% below rated voltage when the pulse rises, and peaks at 52% above rated voltage when the pulse unloads; both peaks being outside acceptable limits. Conversely, when the feedforward loop is included, the voltage is maintained to within ± 0.5% as shown by Fig. 4-61(b), which is within the 10% tolerance prescribed by IEEE 1709. The impact of the feedforward controller is a small overshoot at the leading edge of the output power response shown in Fig. 4-61(d). The effect of this overshoot is explored in chapter 6.
4.14 Summary

This chapter has detailed three modelling tools constructed to conduct analytical and time-domain simulation based research into the performance of the battery ESS in operating modes of power reserve, load levelling, and LDEW power supply in a candidate warship electric power system. Modelling tool 1 uses a steady state representation of the power system to investigate the potential fuel savings, exhaust GHG emission savings and running hour reduction from integrating the battery ESS with the candidate warship power system. Modelling tool 2 is a quasi-steady state simulation model of a DG operating in parallel with a battery ESS to investigate the ability the ESS to load level the DG. Modelling tool 3 is a dynamic simulation tool to investigate the ability of the candidate battery ESS to power anticipated LDEW loads. For each tool the modelling requirements were defined, with the corresponding modelling limitations and assumptions that have been adhered to in this research.

This chapter justified the selection of MATLAB/Simulink as the software package with which to conduct the research. A description of the models used in each modelling tool was then offered, including model schematics or equivalent circuits along with selection and justification of model parameters.

A verification and validation study of the models was carried out to demonstrate that they were suitably representative enough to allow simulation based research to be conducted. It was determined that each model is suitable for use in this research and, where appropriate, applicable to steady-state, quasi-steady state or dynamic representation of the characteristics for the candidate warship power system. A summary of each of the dynamic models’ validity and associated limitations is given below in Table 4.22. The results to analyse the ability of the ESS to operate as a power reserve and load levelling generated by modelling tool 1 and 2 respectively are presented in chapter 5. The results to analyse the ability of the ESS to power the LDEW generated by modelling tool 3 are presented in chapter 6.
### Table 4-22. Simulation model validation and verification summary

<table>
<thead>
<tr>
<th>Model</th>
<th>Status</th>
<th>Limitations</th>
</tr>
</thead>
<tbody>
<tr>
<td>Battery ESS</td>
<td>Validated</td>
<td>Does not include thermodynamic representation of the cell.</td>
</tr>
<tr>
<td></td>
<td></td>
<td>Does not simulate calendar or cyclic aging.</td>
</tr>
<tr>
<td>DG</td>
<td>Verified</td>
<td>Does not include thermodynamic representation of combustion dynamics.</td>
</tr>
<tr>
<td>VSC and control system</td>
<td>Verified</td>
<td>Thermodynamics of the converter and thermal protection schemes are not included in the VSC model.</td>
</tr>
<tr>
<td>Transformer and power cables</td>
<td>Verified</td>
<td>The transformer is a quasi-steady state impedance model.</td>
</tr>
<tr>
<td></td>
<td></td>
<td>The power cables include line impedance only and do not consider shunt-capacitance to earth.</td>
</tr>
<tr>
<td>Power sharing capability</td>
<td>Verified</td>
<td>Synchroniser circuit cannot detect de-synchronisation.</td>
</tr>
<tr>
<td>DC/DC converter and control system</td>
<td>Verified</td>
<td>Thermodynamics of the converter and thermal protection schemes are not included in the DC/DC model.</td>
</tr>
<tr>
<td></td>
<td></td>
<td>The output capacitor model accounts for series resistance but does not consider equivalent leakage resistance.</td>
</tr>
</tbody>
</table>
Chapter 5  Power reserve and load levelling modes

5.1 Introduction

The aim of this chapter is to investigate two research problems. The first, identified for the power reserve ESS operating mode, detailed earlier in section 3.7, is the quantification of steady state performance of the candidate warship power system from the perspective of engine fuel consumption, running hours and exhaust GHG emissions in relation to operating profile, operating configuration and efficiency characteristics. The second research problem that will be investigated pertains to the ESS operating in load levelling mode. The load levelling investigation aims to address the electrical QPS, fuel consumption, exhaust GHG emissions and the impact of engine running hours on load sharing between a DG and the candidate ESS in quasi-steady state conditions. This investigation aims to compare load levelling performance with conventional operation and ESS power reserve operation.

Chapter 3 detailed the design and operating configuration of the hybrid power and propulsion system of the candidate warship under investigation. Justification for the siting of the ESS location within the power system architecture was provided as well as the integration interface of the candidate ESS to the power system. Chapter 4 detailed the development of three modelling tools. Modelling tool 1 was developed as a steady state modelling tool, capable of analysing the performance of the candidate power system when the ESS is operating in power reserve mode. Modelling tool 2 was developed as a quasi-steady state model capable of simulating the load sharing behaviour and QPS of a DG and the candidate ESS when operating in load levelling mode. Modelling tool 3 was developed to investigate the dynamic response and ability of the candidate ESS to facilitate the primary operating mode of LDEW power supply. The dynamic response using Modelling tool 3 will be explored in chapter 6.

This chapter is divided into the following six main parts:

1) Part 1 details the power reserve investigation using modelling tool 1 and justifies the need for the battery ESS to operate as a power reserve.

2) The power reserve investigation results are presented in part 2. This includes the fuel consumption, exhaust GHG emissions and engine running hours implications of operating the ESS in power reserve mode. A CAPEX vs OPEX cost sensitivity study is conducted.
3) The load levelling investigation is justified and described in part 3. The power management framework used in modelling tool 2 is also described.

4) The results of the load levelling investigation are presented in part 4, including the system response, fuel consumption and exhaust GHG emissions performance under different load profiles.

5) Following the analysis of the results of both investigations, the implications and consequences of the results are then discussed in part 5.

6) This chapter concludes with recommendations when considering using the candidate battery ESS in the power reserve and load levelling operating modes.

5.2 Power reserve investigation

5.2.1 Investigation justification

The purpose of this investigation is to determine the extent to which integrating the battery ESS with the candidate power system can increase the generator loading margins when the ESS is operating in the power reserve mode. This could permit the DGs to operate at a higher loading condition and reduce SFC and exhaust GHG emissions. The fuel consumption characteristic of the DG is shown in Fig. 4-41 in section 4.9.2. The number of DGs required on-line could also be reduced if there is sufficient power reserve in the ESS.

The results quantified in this investigation are diesel engine fuel consumption, exhaust GHG emissions and running hours for the two different operating profiles of the candidate warship. The total annual operating time at sea for both profiles is 5,000 hours. The results are quantified for the candidate warship when an ESS is operating in power reserve mode (the concept for which is detailed in chapter 3), and also when there is no ESS integrated. This is to provide a comparison of the system performance with and without ESS. The operating profiles of the candidate warship and operating configurations were presented and justified in section 3.2. A cost sensitivity analysis will be conducted into the initial CAPEX of the battery ESS and interface with the power system versus the OPEX savings, when the ESS is operating in power reserve mode.

The aim of the investigation is to inform naval power system designers to the potential benefits of using a battery based ESS to act as power reserve for the candidate warship power system. The justification for conducting this investigation is to obtain evidence that demonstrates the ability of the ESS to reduce engine fuel consumption, GHG emissions and engine running hours. The results of this investigation will then be used to support and develop recommendations on the limits of operation for the battery ESS. Moreover, this research will contribute to knowledge on the potential capability of the Li-ion ESS to reduce exhaust emissions of warship power systems when operating in power reserve mode.

For the power reserve investigation, modelling tool 1, described in chapter 4 is used. The process diagram of modelling tool 1 is presented in Fig. 5-1. The blue shaded boxes are inputs to the process. The right hand
side of the diagram shows the baseline performance calculations, which determine performance when the spinning reserve of the prime movers is used as power reserve. The left hand side uses the ESS in power reserve mode and, based upon the SoC, the DG constraints are calculated for the operating profiles. The baseline case, and case with ESS are then compared in the post processing stage.

The following sub-section describes the investigation and the assumptions made in quantifying costs and GHG emissions. Results of the investigation are subsequently presented in section 5.3.

![Power reserve modelling tool flow chart](image)

Fig. 5-1. Power reserve modelling tool flow chart

### 5.2.2 Investigation description

The power reserve modelling tool comprises two key parts, the baseline system without ESS and the system with ESS. The steps followed to generate the baseline results are as follows:

1) Following the power reserve method described in section 3.5.3, determine the number of generators online and their loading condition over the operating states, based upon the power system operating configurations described in section 3.2. The allowable number of failed generators is assumed as 1.
2) Calculate running hours, fuel consumption and exhaust GHG emissions over each operating state and combine for each operating profile. This forms the baseline results to compare with the system that includes ESS.

The steps followed to generate the system results with ESS are as follows:

1) The initial step when calculating the results with the ESS involves inputting a range of initial SoC to the modelling tool. The tool then calculates the maximum output power available from the ESS at the switchboard for a 3-minute power ride through.

2) Knowledge of the maximum ESS power allows calculation of the efficiency of the ESS and ESS interface with the power system using the efficiency maps detailed in chapter 4.

3) Following the power reserve method described in section 3.5.3, determine the number of generators online, and their loading condition over the operating states, based upon the power system operating configurations described in section 3.2. As with the baseline system process, the assumed allowable number of failed generators is 1.

4) Calculate the results with ESS included in the candidate power system at each SoC examined.

The post processing stage outputs the results as defined by Table 5-1 when the battery ESS is at BoL conditions.

Table 5-1. Summary of the power reserve investigation and the results sought

<table>
<thead>
<tr>
<th>Investigation</th>
<th>Description</th>
<th>Results sought</th>
</tr>
</thead>
<tbody>
<tr>
<td>Power reserve</td>
<td>Analytically quantify the power system steady state performance with and without the ESS acting as a power reserve.</td>
<td>DG fuel consumption&lt;br&gt;DG fuel cost&lt;br&gt;Total fuel consumption&lt;br&gt;Total fuel cost&lt;br&gt;DG running hours&lt;br&gt;DG maintenance cost&lt;br&gt;Engine GHG emissions&lt;br&gt;Battery ESS and interface capital cost&lt;br&gt;Battery ESS and interface capital cost payback period</td>
</tr>
</tbody>
</table>

5.2.3 Costing

The cost sensitivity analysis considers the trade-off between CAPEX of the battery ESS and ESS interface, and the OPEX of the DG fuel and maintenance cost. Extra cabling and circuit breakers are not included in the calculations, these are assumed negligible relative to the other costs included in the sensitivity analysis. The following assumptions were made to determine the DG maintenance cost and battery ESS interface purchase cost, which were assumed fixed.

1) Engine maintenance costs of £10 per rated MW per hour of use (Buckingham, 2013), this was adjusted for inflation by a factor of 1.12 (Bank of England, 2018).
2) The cost of VSC and transformer is assumed as £89 per kW from Barrera-Cardenas et al. (2019), their work was focused on a commercial application, therefore a 40% cost margin has been added for adapting the equipment for naval application.

The CAPEX of the ESS and OPEX of the DG fuel were adjusted in the sensitivity analysis. This was implemented because these costs could be more susceptible to cost fluctuation as a result of uncertainty. These costs are influenced by a variety of technological, environmental, social and political factors. For example the increase in global sales of EVs has led to high production volumes, translating to a decline in Li-ion system cost due to improved economies of scale (Nykvist and Nilsson, 2015; Schmidt et al., 2017). An advantage of Li-ion cells is their modularity, which means it is possible for them to be used in multiple applications, including EV, utility and marine applications. This could facilitate cost reduction of marine ESS systems resulting from the techno-economic benefits in electric vehicle applications. Marine fuel prices are influenced by IMO legislation such as the global sulphur limit in ships fuel oil (IMO, 2019a), resource availability and demand among other factors.

The Li-ion ESS cost parameters used in the sensitivity study were selected based on reviewing available literature comprising historical and projected costs for marine, electric vehicle and utility Li-ion systems. Marinised system costs have been reported by Alnes et al. (2017) and Bellamy and Bray (2015). Alnes et al. (2017) provide a projection of marinised battery system cost to 2030, but did not discuss the method followed. Schmidt et al. (2017) derived experience curves that follow Wright’s law using historic prices and cumulative installed capacity of Li-ion to project future costs. Nykvist and Nilsson (2015) reviewed literature on previous and projected costs of EV battery systems. Results of each publication are provided in Fig. 5-2 based on 2019 GB £ per kWh of Li-ion capacity installed.

![Fig. 5-2. Historical and projected cost of Li-ion battery systems in EV, utility and marine sectors (Alnes et al., 2017; Bellamy and Bray, 2015; Nykvist and Nilsson, 2015; Schmidt et al., 2017)](image)

Historical prices in EV, marine and utility sectors have been reported to decrease between 2010 and 2016 (Alnes et al., 2017; Schmidt et al., 2017). This was shown by Schmidt et al. (2017) to be a function of increasing cumulative installed capacity. In their cost projections, which are based on a trend of increasing
cumulative installed capacity and amount spent on Li-ion technology, Schmidt et al. (2017) observed that cost reductions are likely to occur and be driven by experience in cell manufacturing rather than other components of the battery system. There is also an increase in demand for Li-ion battery installations in offshore marine vessels, as previously shown in Fig. 2-8 in section 2.2.3, which could be a contributory factor within the cost reductions shown in the projections made by Alnes et al. (2017).

Fig. 5-2 shows that the cost of marinised systems are closest to utility based Li-ion storage, and between utility and EV sectors, but could be higher than utility based storage in the future. Overall, the trend for each application is decreasing in cost. From Fig. 5-2, marinised systems have reduced from £1162 kWh$^{-1}$ to £931 kWh$^{-1}$ between 2013 and 2015. The cost projections made by Alnes et al. (2017) suggests that in 2019, a Li-ion ESS could cost between a minimum of £525 kWh$^{-1}$ to a maximum of £850 kWh$^{-1}$. This is substantially higher than the more mature EV market. Naval application of Li-ion ESS could also increase costs from the projections reported in Fig. 5-2, the degree of which is uncertain. To account for the range of uncertainty in the sensitivity study, five costs parameters are included:

1) A 20% increase in 2015 cost reported by Bellamy and Bray (2015) and Alnes et al. (2017): £1145 kWh$^{-1}$

2) Price equivalent to 2015 from Bellamy and Bray (2015) and Alnes et al. (2017): £955 kWh$^{-1}$

3) Upper bound of 2019 projected cost in Fig. 5 2 from Alnes et al. (2017): £850 kWh$^{-1}$

4) Mean value of 2019 projected cost in Fig. 5 2 from Alnes et al. (2017): £687.50 kWh$^{-1}$

5) Lower bound of 2019 projected cost in Fig. 5 2 from Alnes et al. (2017): £525 kWh$^{-1}$. The minimum ESS cost was confirmed as appropriate by Corvus Energy for commercial marine applications.

The fuel prices used in the sensitivity analysis were selected based on the 5 year cost of Marine Gas Oil (MGO) from 2014 to 2019, as shown in Fig. 5-3 (Ship and Bunker, 2019). Fig. 5-3 shows a substantial difference between maximum and minimum cost per tonne of MGO from £750 to £374 per tonne respectively. To capture sensitivity in the cost analysis, the maximum, minimum and 5 year mean cost of MGO was included.
A summary of the cost parameters used in the sensitivity study is provided in Table 5-2.

Table 5-2. Summary of sensitivity parameters examined

<table>
<thead>
<tr>
<th>Cost parameter</th>
<th>Values examined</th>
</tr>
</thead>
<tbody>
<tr>
<td>Fuel (£ tonne⁻¹)</td>
<td>374, 533, 750</td>
</tr>
<tr>
<td>Li-ion ESS (£ kWh⁻¹)</td>
<td>525, 687.50, 850, 955, 1145</td>
</tr>
</tbody>
</table>

5.2.4 GHG emissions

The 72nd Marine Environmental Protection Committee of the International Maritime Organization (IMO), agreed to reduce the total global shipping emissions by at least 50% over 2008 levels by 2050 (IMO MEPC, 2018). While naval warships are specifically exempt from IMO regulations, governments have identified the urgent need to reduce emissions from their naval fleets. The UK and New Zealand for example, have both passed into law the requirement to bring GHG emissions to net zero by 2050 (Ministry for the Environment, 2019; UK Government, 2019). This puts pressure on their navies to reduce the carbon footprint of their warships. To benchmark the exhaust GHG emissions performance results of this investigation for the candidate warship against internationally recognised IMO targets, the Emission Factor (EF) method defined in the Third IMO Greenhouse Gas Study (IMO, 2015) was used.

EF information for F-76 grade fuel oil that is used on NATO warships is not included in the referenced IMO study therefore, the values for MGO were assumed initially. This was deemed an acceptable initial assumption as warship fuel systems are required to be capable of handling commercial MGO (Ministry of Defence, 2015). To adjust for F-76, a fuel conversion factor (FCF) was used for the fuel sulphur content (max 0.1 % by mass (Ministry of Defence, 2013)). The process to determine the GHG emissions is summarised as follows:
1) Identify energy based EFs in g/kWh (IMO, 2015). The supporting equations and EFs are detailed in Table P-1 of Appendix P.

2) Convert energy based EF to fuel based EF (i.e. g pollutant per g fuel consumed), using (5.1). The baseline EFs were determined at 80% MCR.

\[
EF_{\text{baseline}} \left( \frac{g \text{ pollutant}}{g \text{ fuel}} \right) = \frac{EF_{\text{baseline}} \left( g/kWh \right)}{SFC \left( g/kWh \right)}
\]

3) Apply FCF to NO\textsubscript{X}, Sulphur Oxides (SO\textsubscript{X}) and Particulate Matter (PM), to adjust for F-76 fuel type and sulphur content (see Table P-2 in Appendix P for FCF data).

4) Amend the actual EF to account for the SFC curves of the DG during each operating state. The actual EFs for the DG are summarised in Table 5-3 when the DG is at 80% MCR.

5) Using the product of fuel burnt and EFs per state, summate the emissions over the operating profiles.

Table 5-3. DG emission factors at 80% MCR

<table>
<thead>
<tr>
<th>Emission factor (EF)</th>
<th>DG (kg/tonne of fuel)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Carbon Dioxide (CO\textsubscript{2})</td>
<td>3206.00</td>
</tr>
<tr>
<td>Nitrogen Oxides (NO\textsubscript{X})</td>
<td>9.39</td>
</tr>
<tr>
<td>Sulphur Oxides (SO\textsubscript{X})</td>
<td>9.78</td>
</tr>
<tr>
<td>Particulate Matter (PM)</td>
<td>2.30</td>
</tr>
<tr>
<td>Carbon Monoxide (CO)</td>
<td>2.52</td>
</tr>
<tr>
<td>Methane (CH\textsubscript{4})</td>
<td>0.04</td>
</tr>
<tr>
<td>Nitrous Oxide (N\textsubscript{2}O)</td>
<td>0.16</td>
</tr>
<tr>
<td>Non-Methane Volatile Organic Compounds (NMVOC)</td>
<td>1.87</td>
</tr>
</tbody>
</table>

The emissions calculated and analysed in this research are limited to the operation of the warship only, being those emissions generated by converting fuel to mechanical power via the four DGs in the candidate ship power and propulsion system. The GT was not considered in the emissions analysis, as the integration of the ESS does not directly reduce the GT loading margin and therefore the emissions. Integration of the battery ESS and interface with the power system could impact the GT loading due to the additional mass and volume required within the candidate warship. Therefore, the general arrangement, ship volume and displacement will be affected, the quantification of the impact on these elements of the candidate warship design is in the scope of future research. This investigation does not include those emissions generated during ship manufacture, decommissioning or at any other stage of the ship lifecycle. As detailed in section 4.2.3, it is assumed that the engines in the candidate power system are NO\textsubscript{X} IMO tier III compliant therefore the NO\textsubscript{X} limit is 9n\textsuperscript{0.2}, where n is the rated speed of the engine (IMO, 2015). The method of emission reduction here is confined to utilising the battery ESS in the power reserve operating mode.
5.3 Power reserve investigation results

This section will present the results of the power reserve investigation, following the process described in section 5.2.

5.3.1 Baseline power system results

For the candidate system without ESS installed, the maximum loading margins of the DGs are shown in Fig. 5-4. The corresponding number of generators online per operating state and their loading conditions is presented in Table 5-4. The conditions in Table 5-4 were used to calculate the baseline results for the candidate warship. The number of generators online for each climatic condition is the same for the baseline results. However, the generator loading conditions are highest for winter operation, followed by tropical then temperate conditions. As stated in chapter 3, winter conditions assume a minimum seawater temperature of 0°C, with air temperature of -10°C. Tropical conditions assume a maximum seawater temperature of 30°C and air temperature of 40°C. Temperate conditions assume seawater temperature of 18°C with air temperature of 25°C.

![Chart showing maximum generator loading margins without ESS](image)

Table 5-4. Operating state load demands and corresponding generators online

<table>
<thead>
<tr>
<th>State</th>
<th>Winter demand (MW)</th>
<th>Gens online</th>
<th>Tropical demand (MW)</th>
<th>Gens online</th>
<th>Temperate demand (MW)</th>
<th>Gens online</th>
</tr>
</thead>
<tbody>
<tr>
<td>Manoeuvring</td>
<td>3.18</td>
<td>4</td>
<td>2.93</td>
<td>4</td>
<td>2.68</td>
<td>4</td>
</tr>
<tr>
<td>Patrol</td>
<td>3.75</td>
<td>3</td>
<td>3.50</td>
<td>3</td>
<td>3.25</td>
<td>3</td>
</tr>
<tr>
<td>Transit</td>
<td>5.53</td>
<td>3</td>
<td>5.28</td>
<td>3</td>
<td>5.03</td>
<td>3</td>
</tr>
<tr>
<td>RAS</td>
<td>5.53</td>
<td>4</td>
<td>5.28</td>
<td>4</td>
<td>5.03</td>
<td>4</td>
</tr>
<tr>
<td>High speed</td>
<td>3.00</td>
<td>2</td>
<td>2.75</td>
<td>2</td>
<td>2.5</td>
<td>2</td>
</tr>
<tr>
<td>State 1</td>
<td>3.50</td>
<td>4</td>
<td>3.25</td>
<td>4</td>
<td>3.00</td>
<td>4</td>
</tr>
</tbody>
</table>

Fig. 5-5 shows the baseline annual fuel consumption for the ASW and GP operating profiles for each of the climatic conditions, in descending order of fuel consumption. Fig. 5-5 also includes the GT fuel consumption for comparison. The increased fuel consumption in winter is caused by the increased HVAC demands compared to environmental loading in tropical and temperate climates. The GP profile consumes an average of 1,080 tonnes more fuel per year than the ASW profile. The proportion of fuel that is required
for electrical power generation accounts for 84.7% and 85.5% of the total fuel consumed over the ASW and GP operating profiles respectively. The remaining proportion of the operating profile fuel consumption is required for mechanical propulsion at higher vessel speeds.

Table 5-5 presents the annual baseline running hours and corresponding DG support costs for the candidate ship. As shown in Table 5-4 the number of DGs running in each climatic condition is governed only by the operating state, hence the running hours and support costs related to running hours are equal. The delta in running hours between the ASW and GP winter profile is a consequence of the increased time spent on manoeuvring for the GP profile compared to the ASW profile. During manoeuvring all generators are online and operating under low load condition. Operating state configurations for the baseline system were discussed in section 3.2.

Table 5-5. Baseline running hours and support costs for ASW and GP operating profiles

<table>
<thead>
<tr>
<th>Operating profile</th>
<th>Running hours (thousand hrs)</th>
<th>Support cost (£m)</th>
</tr>
</thead>
<tbody>
<tr>
<td>ASW</td>
<td>15.75</td>
<td>0.529</td>
</tr>
<tr>
<td>GP</td>
<td>15.65</td>
<td>0.526</td>
</tr>
</tbody>
</table>

5.3.2 Power system results with ESS

This section presents the results with ESS in the candidate warship power system. First the efficiency map of the ESS and ESS interface is presented, the loading margins of the generators are then determined. Finally the results are presented for each operating profile.

ESS interface efficiency map

Fig. 5-6 presents the discharge efficiency map of the battery ESS as a function of the maximum discharge current and SoC. The efficiency map includes the losses from the battery ESS to the main switchboard via the VSC, LCL filter, transformer and the losses due to waste heat of the cells in the battery ESS. The
efficiency of the ESS and interface follows a concave characteristic with discharge current. The efficiency is 85% in the low power range, rising to ~90% at the mid power range before falling at higher power. Within the 90-20% SoC operating range of the battery, the minimum efficiency is 84% at 0.5 C discharge, while peak efficiency is 91.5% when operating at 2 C discharge or charge. The efficiency is low at low load of the ESS due to the VSC and transformer efficiency curve characteristics, as presented in section 4.7 and 4.8.

Fig. 5-6. Battery ESS efficiency measured at the distribution bus under discharge

The maximum power available at the forward and aft switchboards for each ESS, cognisant of losses, is presented in Table 5-6. The ESS SoC limits were set at 90% and 20% to limit impact on battery life (Hannan et al., 2018). Due to the 3-minute power reserve duration and SoC constraints, the minimum allowable initial SoC for the ESS to act as power reserve is 50%. If the ESS violates this condition the battery will be depleted to below 20% SoC. Subsequently the initial SoC inputs to the model were set to 90%, 70% and 50% to determine the ESS power limits when functioning as power reserve.

Table 5-6. Maximum power available at the main switchboard from each ESS unit.

<table>
<thead>
<tr>
<th>Initial SoC at ride through (%)</th>
<th>Efficiency from efficiency map in Fig. 5-6 (%)</th>
<th>Power available at the bus during 3 minute ride through (MW)</th>
</tr>
</thead>
<tbody>
<tr>
<td>90</td>
<td>88</td>
<td>2.55</td>
</tr>
<tr>
<td>70</td>
<td>88</td>
<td>2.47</td>
</tr>
<tr>
<td>50</td>
<td>85</td>
<td>2.34</td>
</tr>
</tbody>
</table>

Generator loading margins

In closed bus configuration of the switchboards, the available power from the ESS is theoretically doubled from Table 5-6 due to the two battery ESS units. Therefore, using the power reserve methodology, the loading margins of the generators in the operating states that use a closed bus configuration are 100% for the N-1 failure condition. This is because the minimum available power, after losses, from the combination of the two ESS is 4.68 MW at the lowest initial SoC condition of 50%.

In split bus configuration the loading margin per generator is as shown in Fig. 5-7, where the ESS is
dedicated to be a power reserve (idle but not providing load) for the respective forward and aft switchboards. Unlike the closed bus configuration, the loading margins are not 100%. This is due to the power available at each switchboard being less than the DG real power rating of 3 MW, as detailed in Table 5-6.

The number of generators online per operating state is presented in Table 5-7. The reduction in the number of running generators of the system with ESS against the baseline system was the same for the 50%, 70% and 90% initial SoC conditions.

### Table 5-7. Generators online in each operating state with and without ESS

<table>
<thead>
<tr>
<th>State</th>
<th>Winter Without ESS</th>
<th>Winter With ESS</th>
<th>Tropical Without ESS</th>
<th>Tropical With ESS</th>
<th>Temperate Without ESS</th>
<th>Temperate With ESS</th>
<th>Bus configuration</th>
</tr>
</thead>
<tbody>
<tr>
<td>Manoeuvring</td>
<td>4</td>
<td>2</td>
<td>4</td>
<td>2</td>
<td>4</td>
<td>2</td>
<td>Split</td>
</tr>
<tr>
<td>Patrol</td>
<td>3</td>
<td>2</td>
<td>3</td>
<td>2</td>
<td>3</td>
<td>2</td>
<td>Closed</td>
</tr>
<tr>
<td>Transit</td>
<td>3</td>
<td>2</td>
<td>3</td>
<td>2</td>
<td>3</td>
<td>2</td>
<td>Closed</td>
</tr>
<tr>
<td>RAS</td>
<td>4</td>
<td>4</td>
<td>4</td>
<td>4</td>
<td>4</td>
<td>4</td>
<td>Split</td>
</tr>
<tr>
<td>High speed</td>
<td>2</td>
<td>1</td>
<td>2</td>
<td>1</td>
<td>2</td>
<td>1</td>
<td>Closed</td>
</tr>
<tr>
<td>State 1</td>
<td>4</td>
<td>4</td>
<td>4</td>
<td>4</td>
<td>4</td>
<td>4</td>
<td>Split</td>
</tr>
</tbody>
</table>

### Running hours, fuel consumption and GHG emissions

The impact on running hours, fuel consumption and exhaust GHG emissions of integrating the ESS with the candidate vessel, and operating the ESS in power reserve mode, is presented in Fig. 5-8(a) and Fig. 5-8(b) for the ASW and GP profiles respectively. The battery ESS in power reserve mode reduces the running hours of the engines of the ASW profile by 29.7% regardless of climatic condition. This is compared to the baseline of 15,650 running hours for the ASW profile. The reduction for the GP profile is marginally higher at 30.2% against a baseline of 15,750 running hours.

The mean DG fuel consumption reduction for the ASW profile is 35.8% against a baseline of 16,256 tonnes. The total fuel consumption reduction, for the candidate warship in ASW profile after the fuel consumption of the GT is accounted for, is 30.3%. The GP profile average DG fuel consumption reduction is 35.9%
against a baseline of 17,337 tonnes of fuel. The total reduction in fuel consumption for the GP profile with the GT averages at 30.7%. The average GHG emissions reduction for the ASW and GP profiles are 40.7% and 40.6% respectively. For both profiles the winter emissions were higher due to the total hotel of 3 MW compared to 2.75 MW and 2.5 MW during tropical and temperate conditions respectively.

Fig. 5-8. Comparison of when the ESS functions as power reserve against the baseline candidate power system performance under (a) ASW and (b) GP operating profiles

5.3.3 Cost sensitivity analysis

Thus far, the power reserve case study results have demonstrated that, based on the two operating profiles in this case study, there are significant savings in fuel and engine running hours against the baseline system performance. Reducing the engine running hours would also provide the opportunity to increase the MTBO, which could reduce the maintenance cost burden of the prime movers. The cost analysis considers the extent to which the initial CAPEX could be offset by the OPEX savings that the ESS introduces to the warship power system. The OPEX, which is the combined total of DG fuel consumption and maintenance costs, for the baseline system and system with ESS is presented in Fig. 5-9 for each fuel price. The fuel prices for MGO were summarised in Table 5-2. The higher fuel consumption of the GP profile compared with the ASW profile is reflected in the OPEX results when comparing Fig. 5-9(b) and (a).

Fig. 5-9. OPEX results of the baseline system and system with ESS during the (a) ASW and (b) GP operating profiles
Knowledge of the OPEX savings from the results in Fig. 5-9, allows calculation of the payback period using the CAPEX costs. The subsequent payback period results are presented in Fig. 5-10 for the varying fuel OPEX cost and ESS CAPEX cost. The costs were previously summarised in Table 5-2 but the different cost groupings have been highlighted in Fig. 5-10.

As the OPEX savings are highest under winter operating conditions, the winter payback period is shorter than the tropical and temperate operating condition results. As expected, the worst-case payback period occurs for the situation of minimum fuel cost and highest CAPEX, which is 5.4 years under temperate conditions. Conversely, the best case is 1.3 years at highest fuel cost and minimum CAPEX under winter conditions.

The factor that needs to be considered next is ESS lifetime, which is influenced by a complex combination of calendar and cycle life factors (Birkl et al., 2017; de Hoog et al., 2018, 2017). The cycle life of NMC based batteries is approximately 4000 cycles when discharged to 50% SoC (de Hoog et al., 2018), 50% SoC is the lower start of operation limit when operating in power reserve in this investigation. Using the estimate of ESS cycle life, the maximum number of cycles per year before a replacement is required, can be calculated. This characteristic is important because it informs the ship’s owner as to the limit of the number of recharge - discharge cycles, before the OPEX savings no longer outweigh the CAPEX.

To identify the number of cycles per annum would require specific power vs time profiles, which by virtue of warship operations, is sensitive information. Stevens et al. (2017) estimated that the cycle range per year may range between 430 and 1,075 cycles for 4,000 hours of operation. When scaled to 5,000 operating
hours, as implemented here, this estimate increases to between approximately 540 and 1,350 cycles per year. A cycle range of between 100 and 2,000 cycles per year has been applied in this analysis to expand the potential range of cycles. The time to replacement of the cells in the system is calculated using (5.2) and plotted in Fig. 5-11.

\[
\text{Years to replenishment} = \frac{\text{Max cycles}}{\text{cycles}}
\]  
(5.2)

Fig. 5-11. Estimated time to replenishment based upon the number of cycles to 50% SoC per year

Fig. 5-11 can be used to calculate the maximum number of cycles to determine at which point there is CAPEX and OPEX breakeven over the life of the ESS. This is calculated using (5.3) and plotted for the ASW and GP results in Fig. 5-12 and Fig. 5-13 respectively.

\[
\text{Maximum cycles per year} = \sqrt{-\frac{\text{Payback period}}{\text{Maximum number of cycles}}}
\]  
(5.3)

The results in Fig. 5-12 show how the payback period results in Fig. 5-10 compare with the maximum number of cycles per year to allow breakeven between the CAPEX and OPEX savings. It is evident that the low fuel price limits the number of cycles to below 1,750. If the CAPEX of the ESS increases from 2015 prices this reduces to below 1,000 cycles per year, which is below the mean cycles per year reported in Stevens et al., (2017). However, for the predicted cost boundaries, highlighted in green, in Fig. 5-12 the mean and high fuel price present favourable cycle and payback characteristics. If costs are to reduce as projected in Fig. 5-2, the payback period of the second replenishment will be lower than the results presented in this analysis. Moreover the £750,000 cost of replacement of the VSC and transformer might not be an imposition.
5.3.4 Discussion

The impact of utilising the ESS as a power reserve will be explored here in more detail using the winter climatic condition results, as these present the worst case scenario from a power consumption perspective. This discussion concludes by identifying the contributions that this power reserve case study has made to the field by analytically quantifying power system performance for a candidate warship with respect to the ship operating profile, power system line up and efficiency characteristics of the ESS interface.
Fig. 5-14 shows the impact of the ESS on engine loading compared to the case without ESS, in accordance with operating states of the vessel under winter condition. A summary of the generators online with the corresponding engine loading is provided in Table 5-8. The equivalent results for temperate and tropical conditions are included in Appendix Q. When the ESS is integrated with the candidate power system the engine loading increases by a minimum of 20% for all the operating states, except RAS and State 1. In State 1 the available power of the ESS is dedicated to LDEW operation. During RAS operations, the power system is operating in split bus configuration, therefore each ESS is limited to 2.55 MW output power when at 90% SoC on each switchboard, which is not sufficient to reduce the requirement for the number of generators running from two to one per switchboard. However, in all other operating states, where cumulatively the candidate warship spends approximately 87% of its operating time, the diesel engine loading is increased, whilst reducing the number of generators online. This is the reason for the approximate 36% savings in fuel consumption for both the GP and ASW profiles.

![Fig. 5-14. Individual DG and GT MCR during each operating state under winter conditions with and without ESS](image)

Table 5-8. Summary of the number of generators online and their loading condition per operating state without and with ESS operating in power reserve mode

<table>
<thead>
<tr>
<th>Operating state</th>
<th>Without ESS</th>
<th>With ESS</th>
<th>Bus configuration</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Generators online</td>
<td>Engine loading (%)</td>
<td>Generators online</td>
</tr>
<tr>
<td>Manoeuvring</td>
<td>4</td>
<td>28.3</td>
<td>2</td>
</tr>
<tr>
<td>Patrol</td>
<td>3</td>
<td>40.9</td>
<td>2</td>
</tr>
<tr>
<td>Transit</td>
<td>3</td>
<td>63.6</td>
<td>2</td>
</tr>
<tr>
<td>RAS</td>
<td>4</td>
<td>48.1</td>
<td>4</td>
</tr>
<tr>
<td>High speed</td>
<td>2</td>
<td>50.0</td>
<td>1</td>
</tr>
<tr>
<td>State 1</td>
<td>4</td>
<td>30.8</td>
<td>4</td>
</tr>
</tbody>
</table>

The corresponding fuel burnt by the DGs for each operating profile and operating state is shown in Fig. 5-15. The reduction in fuel burnt by the DGs reflect the increase in engine loading per operating state in
Fig. 5-14 and the reduction in the number of running generators shown in Table 5-7. From the consumption characteristic of the DG in Fig. 4-44(a), it can be deduced that the greatest benefits to fuel consumption performance can be achieved by increasing the engine loading margin within the lower power range of the engine. This is reflected in the results of the ASW operating profile and can be shown by comparing the transit and patrol operating state results. Consider the engine loading and fuel burnt during patrol and transit states of the ASW profile in Fig. 5-14 and Fig. 5-15 respectively. The loading increase in the patrol state is from 40% to 60% of MCR, whereas the transit loading margin increase is 31%, being from 64% to 95% MCR. Cross-examination of these figures highlights an important result. The delta between the fuel burnt during the patrol and transit states reduces from 11% for the baseline result, to 3% when the ESS is integrated. This is primarily a consequence of the improvements in the lower loading range of the engines during patrol.

Fig. 5-15. DG fuel burn during each operating state with and without ESS under winter conditions

5.3.5 Power reserve investigation summary

This investigation has advanced on the power reserve method presented by Kim et al. (2015), Radan et al. (2016) and Southall and Ganti (2018) by including higher fidelity steady state models of the power system and taking into account the power system configurations during each operating state. This allows the actual power delivery capability of the ESS at the main switchboard to be determined. The method followed here also addresses a comment previously made by Hodge and Mattick (1999), on the importance of determining when an energy store can or cannot be used in power reserve mode with regard to the warship operating state.

The results in section 5.3.2 and 5.3.3 are summarised in Table 5-9 and Table 5-10 for the ASW and GP profiles respectively. The results have demonstrated how the candidate ESS, when operating in the power reserve mode, is able to reduce the fuel consumption, total engine GHG emissions and engine running hours over the operating profiles of the candidate warship. The total exhaust GHG emissions and pollutants is the sum of the compounds detailed in Table 5-3.
Table 5-9. Summary of ASW operating profile results

<table>
<thead>
<tr>
<th>Performance criteria</th>
<th>Winter Baseline</th>
<th>Winter With ESS</th>
<th>Tropical Baseline</th>
<th>Tropical With ESS</th>
<th>Temperate Baseline</th>
<th>Temperate With ESS</th>
</tr>
</thead>
<tbody>
<tr>
<td>Total DG fuel consumption (tonnes)</td>
<td>16,960</td>
<td>-35.5%</td>
<td>16,264</td>
<td>-36.1%</td>
<td>15,544</td>
<td>-36.0%</td>
</tr>
<tr>
<td>Total Engine running hours</td>
<td>15,650</td>
<td>-30.0%</td>
<td>15,650</td>
<td>-30.0%</td>
<td>15,650</td>
<td>-30.0%</td>
</tr>
<tr>
<td>Total engine GHG emissions (tonnes)</td>
<td>66,652</td>
<td>-40.2%</td>
<td>63,553</td>
<td>-40.8%</td>
<td>61,940</td>
<td>-41.2%</td>
</tr>
</tbody>
</table>

Table 5-10. Summary of GP operating profile results

<table>
<thead>
<tr>
<th>Performance criteria</th>
<th>Winter Baseline</th>
<th>Winter With ESS</th>
<th>Tropical Baseline</th>
<th>Tropical With ESS</th>
<th>Temperate Baseline</th>
<th>Temperate With ESS</th>
</tr>
</thead>
<tbody>
<tr>
<td>Total DG fuel consumption (tonnes)</td>
<td>18,028</td>
<td>-35.4%</td>
<td>17,344</td>
<td>-36.0%</td>
<td>16,643</td>
<td>-36.4%</td>
</tr>
<tr>
<td>Total Engine running hours</td>
<td>15,650</td>
<td>-30.2%</td>
<td>15,650</td>
<td>-30.0%</td>
<td>15,650</td>
<td>-30.0%</td>
</tr>
<tr>
<td>Total engine GHG emissions (tonnes)</td>
<td>68,845</td>
<td>-40.0%</td>
<td>65,801</td>
<td>-40.6%</td>
<td>64,284</td>
<td>-41.3%</td>
</tr>
</tbody>
</table>

This investigation demonstrated that due to the power available from the ESS at the switchboards, the number of running generators was reduced compared to the baseline results case for all operating states. The exception being with RAS where the power capability of the ESS was not sufficient to reach the safe loading margin to reduce from two generators to one generator per switchboard. The ESS is dedicated to provide power for LDEW operation when the ship is in State 1, therefore the number of generators online and their loading margins did not change from the results without ESS. It was shown that the engine loading increases by a minimum of 20% in the remaining operating states for the winter climatic conditions. This had positive implications for the fuel consumption characteristic of the ship, in that the reduction in fuel consumption when the ESS is operating in power reserve mode was 36%. The largest reduction was found to be in the exhaust emissions, being 41.3% less than the baseline system over the GP profile.

5.4 Load levelling investigation

5.4.1 Investigation justification

The purpose of this investigation is twofold. First, to investigate the electrical performance on the main switchboard during load levelling using modelling tool 2, the development of which was detailed in chapter 4. Second, to quantify the potential fuel consumption and exhaust GHG emissions when the battery ESS operates to load level a DG in the candidate warship power system. The system is detailed in section 3.2. As discussed in section 2.2.3 and Table 3-5 in section 3.4, load levelling in the context of this research, is using the candidate ESS to decouple the load from the DG. This is achieved by the ESS supplying or absorbing power during load fluctuations, therefore allowing the DG to operate at its optimum efficiency point of 85% MCR. The first part of this investigation explores the interaction between the DG and ESS when operating in parallel from the perspective of QPS and power sharing. This is a quasi-steady state investigation, which investigates the ability of the ESS and DG to maintain the power system within the
steady state tolerances defined by NATO STANAG 1008. In section 3.7, NATO STANAG 1008 was identified as the baseline QPS standard for AC low voltage warship electrical power systems. As identified in Table 3-5, the justification for conducting this investigation is that there is limited published information on the electrical system performance when operating ESS and generators in parallel under load levelling in warships with LV AC power distribution systems.

This part of the load levelling investigation could provide valuable information to power system designers, operators and naval power system design standards. First the results will demonstrate whether the designed ESS interface, when operating in parallel with a DG, is capable of maintaining QPS on the main distribution bus under quasi-steady state conditions. In conventional warship power systems, power generation is provided by prime movers, driving synchronous generators to provide fixed frequency AC electrical power, as discussed in section 2.1.1. However, NATO STANAG 1008 is yet to include consideration for QPS when conventional power generation sources operate in parallel with ESS, therefore the results will demonstrate the level of adherence to the performance standard. Furthermore, the results will form the basis of discussion on the application of NATO STANAG 1008 to warships where an ESS is acting as a significant source in parallel with conventional power generation technology.

The second part of the investigation concerns engine fuel consumption and exhaust GHG emissions from the candidate warship. The results of this investigation can be used to identify the possible savings available in fuel consumption and the reduction in exhaust GHG emissions from the candidate warship against current practice. Therefore, the results could be relevant to ship operators for mission planning with regard to the level of predicted fuel consumption. This will also be relevant to tankage requirements, general arrangement and ship stability from a naval architecture perspective.

5.4.2 Investigation description

For each part of the investigation, modelling tool 2, detailed in section 4.3 and shown in Fig. 5-16, was used to obtain results. Fig. 5-16 also highlights the results sought from the investigation. The power management framework in the tertiary layer of the system is detailed in the following sub-section.

Five load profiles were simulated using modelling tool 2. The load profiles are shown in Fig. 5-17. Each profile is of six minutes in duration, the length of which is a trade-off between computational time and the results sought from the investigation for electrical system performance. If the latter were not of interest, longer duration load profiles would be more appropriate. The load profiles were designed to allow for a 5 s period for initialisation, followed by 20 s to ensure stable synchronisation of the power system during the simulation. At 25 s variable load is applied to the model. Prior to synchronisation the ESS discharges to a local load on the star side of the transformer prior to the interconnecting breaker that connects the ESS to the main switchboard of the power system. The local load for each profile was a 50 kW, 0.85 PF lagging load that remained connected and drawing power for the duration of the simulation.
The variation in load is up to 1 MW for each profile in Fig. 5-17. The differences between the profiles are frequency of load change and ramp rate of the load. Profiles 1 and 4 have a relatively higher occurrence of load variation, which may be representative of a condition such as high sea states. Profiles 2, 3 and 5 are less variable, however they demonstrate larger changes in load and could be representative of ramp up and ramp down of the ship’s propulsion motors for short periods during the patrol operating state. An element of load noise is included in load profile 2. This was included to test the steady-state stability of the system.

For the load profiles in this investigation, power reserve convention for the N-1 generator failure case dictates that without an ESS in power reserve, two DGs would be online and supplying the ship’s load. When the proposed ESS is integrated with the ship’s power system in power reserve mode, the number of DGs online would be one. Therefore, for each load profile, three simulations were conducted. One with the ESS load levelling the DG, one with two DGs operating in parallel, and one with a single DG providing the load demand with the ESS is operating in power reserve. This provides two useful comparisons against conventional naval practice of using a minimum of two generators in parallel, as discussed in section 2.2.2. Table 5-11 provides a summary of the tests undertaken and results sought from the load levelling investigation.

Fig. 5-16. Modelling tool 2, identifying results sought from QPS and fuel investigation
Table 5-11. Summary of load levelling investigations and results sought

<table>
<thead>
<tr>
<th>Investigation</th>
<th>Load levelling test condition</th>
<th>Results sought</th>
</tr>
</thead>
<tbody>
<tr>
<td>QPS</td>
<td>1 DG, 1 ESS</td>
<td>Main bus frequency (Hz)</td>
</tr>
<tr>
<td></td>
<td>2 DGs only</td>
<td>Main bus voltage (V)</td>
</tr>
<tr>
<td></td>
<td>1DG only</td>
<td>DG real power (W)</td>
</tr>
<tr>
<td></td>
<td></td>
<td>DG reactive power (VAr)</td>
</tr>
<tr>
<td></td>
<td></td>
<td>ESS real power (W)</td>
</tr>
<tr>
<td></td>
<td></td>
<td>ESS reactive power (VAr)</td>
</tr>
<tr>
<td>Fuel and GHG emissions</td>
<td>1 DG, 1 ESS</td>
<td>Mass of fuel burned (kg)</td>
</tr>
<tr>
<td></td>
<td>2 DGs only</td>
<td>GHG emissions (kg)</td>
</tr>
<tr>
<td></td>
<td>1DG only</td>
<td>ESS SoC (%)</td>
</tr>
</tbody>
</table>

![Load levelling load profiles investigated](image)

Fig. 5-17. Load levelling load profiles investigated
5.4.3 Power management framework

Modelling tool 2 uses a multi-level power management structure, as shown in Fig. 4-45 in section 4.11. The tertiary layer is the supervisory layer that implements the power management framework. The power management framework uses rule based control and allocates power set points to the battery and DG power sources in the model. The nomenclature adhered to in the power management framework is as detailed in the nomenclature section at the beginning of this thesis.

The aim of the rule based control strategy at each time step is to maintain the DG at its optimum fuel efficiency operating point of 85% MCR (see Fig. 4-44 in section 4.9) whilst the ESS compensates for any surplus or deficit in load demand. This is subject to the current and SoC constraints of the ESS. The current and voltage measurements are fed back to the power management layer from the local measurements of the ESS to ensure that the ESS does not exceed the predefined constraints. The sign convention of the power set point to the ESS, as described in Table 5-12 is that when $P_{ESS} > 0$, the ESS is discharging, when $P_{ESS} < 0$, the ESS is charging. See Nomenclature for the definitions that apply to the variables in Table 5-12.

<table>
<thead>
<tr>
<th>Power</th>
<th>Load condition</th>
<th>$SoC_{min} &lt; SoC_{ESS} &lt; SoC_{max}$</th>
</tr>
</thead>
<tbody>
<tr>
<td>$P_L = P_{DG-opt}$</td>
<td>$P_{ESS^<em>}^</em> = 0$</td>
<td>$P_{DG^<em>}^</em> = P_L$</td>
</tr>
<tr>
<td>$P_L &lt; P_{DG-opt}$</td>
<td>$P_{DG^<em>}^</em> = P_{DG-opt}$</td>
<td>$P_{ESS^<em>}^</em> = P_L - P_{DG}$</td>
</tr>
<tr>
<td>If $I_{ESS} &gt; I_{charge, max}$</td>
<td>$I_{ESS^<em>}^</em> = I_{charge, max}$</td>
<td>$P_{ESS^<em>}^</em> = I_{ESS^<em>}^</em> V_{ESS}$</td>
</tr>
<tr>
<td>$P_{DG-opt} &lt; P_L$</td>
<td>$P_{DG^<em>}^</em> = P_{DG-opt}$</td>
<td>$P_{ESS^<em>}^</em> = P_L - P_{ESS}$</td>
</tr>
<tr>
<td>If $I_{ESS} &gt; I_{discharge, max}$</td>
<td>$I_{ESS^<em>}^</em> = I_{discharge, max}$</td>
<td>$P_{ESS^<em>}^</em> = I_{ESS^<em>}^</em> V_{ESS}$</td>
</tr>
<tr>
<td>$Q_{DG} = Q_L$</td>
<td>$Q_{ESS}^* = Q_{initial}^*$</td>
<td>If $Q_{DG} &gt; Q_{DG,max}$</td>
</tr>
<tr>
<td>$Q_{ESS}^* = Q_{initial}^* + Q_L - Q_{DG}$</td>
<td>$P_{DG^<em>}^</em> = P_L - P_{ESS}$</td>
<td></td>
</tr>
</tbody>
</table>

The reactive power demand is provided by the DG. This is achieved by allocating the reactive power set point, $Q_{DG^*}$, to the droop controller of the DG. The droop controller then imposes the local voltage set point to the AVR. In the power management logic, $Q_{DG^*}$ is constrained to be within the capability plot of the generator. This has been reproduced from the manufacturer datasheet (AvK-Alternators, 2014) and plotted in Fig. 5-18. The capability diagram shows the generator reactive power in MVARs against real power in MW. Under excited and over excited AVR settings refer to leading and lagging power factor conditions.
respectively, as shown by the dashed lines in Fig. 5-18. The real power of the generator is limited by the prime mover and the power factor. The generator capability is bounded by three key limits, the field current, the stator current and stability as shown in Fig. 5-18. While the field current and stator current limits are a function of the thermal limits of the field and stator windings resulting from $I^2R$ thermal heating, it is recognised that iron losses are also present. The third region is the stability limit. The bounds of the curve implemented in modelling tool 2 constrain the power management set points of $Q$ and $V$ to ensure that the generator does not exceed these limits.

![Graph showing the generator capability limits](image)

Fig. 5-18. Generator capability diagram

### 5.5 Load levelling investigation results

This section presents the results of the load levelling investigation. First the power system response is analysed for each load profile with respect to real and reactive power, bus frequency and bus voltage. The QPS during each of the profiles is assessed in context of NATO STANAG 1008 for system steady state response. The second sub-section compares the response of the DG during the three test conditions. These being: 1 DG with ESS, 1 DG only and 2 DGs in parallel. This is followed by an exploration of the fuel consumption and exhaust GHG emissions for each load profile under the three test conditions.

#### 5.5.1 Power system response and QPS when the ESS is in load levelling mode

Fig. 5-19 shows the simulated response of the power system when the ESS performs load levelling of the DG when subject to load profile 1. After 25 s, the load begins to vary until the end of the simulation at 360 s. During this time the DG is maintained at a constant real power output of 2.4 MW which is 85% of its MCR, as shown in Fig. 5-19 (a). During this period the ESS handles the fluctuations in real power by discharging when the load is above 2.4 MW, as shown when $P_{ESS}$ is positive. The opposite occurs when the load is below 2.4 MW when the ESS is charged, as shown when $P_{ESS}$ is negative. This aligns with the real power element of the rule based control strategy. The frequency response of the system at the main switchboard is constant and stable at 60Hz as shown by Fig. 5-19 (c).
Fig. 5-19 (b) shows the reactive power response of the system. The reactive power set point of the ESS was held constant for duration of the simulation, as detailed by the power management framework in Table 5-12. However, inspection of Fig. 5-19 (b) shows that the response of $Q_{ESS}$ is not constant. Point A in Fig. 5-19 (b) indicates the moment at which the power sources are synchronised and load sharing begins. At this stage $Q_{ESS}$ injected by the VSC increases whilst $Q_{DG}$ reduces. When $Q_L$ increases, $Q_{ESS}$ decreases. The opposite applies to $Q_{DG}$. The voltage response on the main switchboard, which is related to the reactive power of the system, is shown in Fig. 5-19 (d). The voltage is maintained within steady state tolerance limits of ± 5% as mandated by NATO STANAG 1008. The profile of the voltage response follows the response of $Q_{ESS}$.

Fig. 5-19. Parallel system (a) real power (b) reactive power (c) system frequency and (d) system voltage response to load profile 1

Fig. 5-20 presents the simulated response of modelling tool 2 when subjected to load profile 2. The frequency and voltage remain within the steady state tolerance limits throughout the simulation as shown by Fig. 5-20 (c) and (d) respectively. Point A in Fig. 5-20 highlights a relatively short period of oscillation that the system undergoes for real and reactive power, frequency and voltage. This occurs between 80 s and 100 s when the ESS is being charged by the DG at 300 kW. The oscillations exhibited cease when the ESS begins to charge when the load demand ramps up to 3 MW immediately after point A.
The response of the system to charging during profile 3 in Fig. 5-21, follows the same trend as the response for profile 2. This is shown by point A and point B, when the ESS is being charged by the DG. The frequency and voltage response in Fig. 5-21 (c) and (d) respectively, are still maintained within NATO STANAG 1008 limits. Point B is magnified for further inspection in Fig. 5-22.
Inspection of Fig. 5-22 (b) and Fig. 5-22 (d) shows that the first oscillations in the reactive power occur between the DG and ESS, which is linked to the system voltage response, the oscillations first occur prior to 200 s. Subsequently the real power of the DG begins to oscillate after 200 s, which causes slight deviations in system frequency of ± 0.2% about 60 Hz, as shown in Fig. 5-22 (a) and Fig. 5-22 (c) respectively. The delay in the real power response to the oscillations is due to the difference in speed of response to load changes of the governor and AVR. The governor regulates the prime mover speed slowly relative to the fast acting response of the AVR. The magnitude of the oscillations in reactive power and voltage do not increase between 220 s and 245 s, Fig. 5-22 demonstrates that the system does not become unstable and remains relatively stable at 690 V ± 10 V.

Fig. 5-22. Magnification of point B in profile 3 to show (a) real power (b) reactive power (c) system frequency and (d) system voltage oscillation response to load change

Fig. 5-23 shows the system response to load profile 4. System frequency is maintained at a constant 60 Hz throughout the simulation as shown in Fig. 5-23 (c) whilst the ESS handles the real power load fluctuations and the DG is maintained at 85% MCR. The system response to load profile did not exhibit the same oscillatory response that was shown in Fig. 5-21 and Fig. 5-22 for load profile 3 when the ESS is being charged. System frequency and voltage are maintained within NATO STANAG 1008 steady state limits as shown in Fig. 5-23 (c) and (d). The voltage response of the main switchboard, similar to Profiles 1, 2 and 3 follows the ESS reactive power response to load changes.
Fig. 5-23. Parallel system (a) real power (b) reactive power (c) system frequency and (d) system voltage response to load profile 4

Fig. 5-24 shows the system response to load profile 5, which displays the least variability of the load profiles examined in this investigation. The ESS is used to manage the real power fluctuations as shown in Fig. 5-24 (a). As with load profiles 1 through 4, there are oscillations in the system response when the ESS is being charged between 240 and 300 s. The power dedicated to charging from the generator output is 400 kW. The DG provides the main proportion of the reactive power demand and the voltage remains stable for the first 235 s of the simulation, at which point the reactive power injected by the ESS oscillates as shown in Fig. 5-24 (b). This manifests in the voltage response on the main switchboard as shown in Fig. 5-24 (d).

Fig. 5-24. Parallel system (a) real power (b) reactive power (c) system frequency and (d) system voltage response to load profile 5

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5.5.2 Comparison of load levelling with DG only results

This subsection compares system response for when the ESS is being used for load levelling of the DG against when the ESS is operating in power reserve mode. Fig. 5-25 compares the frequency and voltage response when the ESS is load levelling with power reserve and parallel DG operation during profile 1. From the scale of the frequency axis in Fig. 5-25(c), it is clear that the frequency is maintained within ±0.2% of 60 Hz for each of the three test cases. The single DG case shows the highest deviations in frequency, due to the reduced inertia available in a single generator set compared to parallel DGs, and DG with ESS.

The busbar voltage when the ESS is used for load levelling is maintained within ± 1.5% of 690 V rms as shown in Fig. 5-25 (d). Comparatively, the busbar voltage during the power reserve mode and parallel DG cases is maintained within ± 0.5% of 690 V. Fig. 5-26 presents the voltage response to the reactive power demand of the load for the five load profiles for the load levelling, power reserve and parallel DG cases.

Each of the voltage responses in Fig. 5-26 is maintained within the steady state tolerance limits of 5% as required by NATO STANAG 1008. However, the system response when the ESS is not in load levelling mode, is within a much tighter tolerance than the case with ESS as shown in the right hand plots of Fig. 5-26.

The presence of noise in load profile 2 is more evident in the voltage response plotted in Fig. 5-26 (d). Noise in the profile did not exhibit any adverse effects on power system response. The oscillations during 70 < t < 100 s in the load levelling case (shown in Fig. 5-26 (d)) are exhibited during the recharge of the
ESS as discussed previously in section 5.5.1.

All voltage peaks in Fig. 5-26 for the load levelling case are higher in magnitude than without the ESS. This is caused by the conflict between the control action of the generator AVR and the primary layer control system of the VSC, which impacts on the supply of reactive power. The parallel DG operation does not suffer this issue, the supply of reactive power is equally split by the control settings of the two generators, enabling a faster acting response compared to the power reserve and load levelling cases. As a consequence the deviations from rated voltage are comparatively lower.

Fig. 5-26 Reactive power demand and voltage response for load profile (a) and (b) 1, (c) and (d) 2, (e) and (f) 3, (g) and (h) 4, and (i) and (j) 5 respectively.
5.5.3 Discussion of system response

The aim of the first part of the load levelling investigation was to assess the ability of the power system to maintain QPS response during quasi-steady state conditions within the limits defined by NATO STANAG 1008. This has been demonstrated for the QPS requirements of voltage and frequency.

The results presented thus far for the system response have shown that the ESS compensates for fluctuation in the real power demand of each load profile, whilst the DG maintains a constant output power of 85% MCR, as mandated by the rule based control power management framework as detailed in Table 5-12. The frequency response on the distribution bus when the power sources are synchronised and sharing load is maintained within ± 0.2% of 60 Hz. This is well within the steady state frequency tolerance of ± 3% required by NATO STANAG 1008.

Converse to the real power sharing performance of the system, the reactive power did not follow the rule based control power management framework precisely. The reactive power injected by the VSC was not held constant as was the intent for each of the load profiles simulated. An inverse response to reactive power demand was exhibited by the VSC, where the increase in the reactive power of the load reduces the amount of reactive power injected via the VSC, simultaneously the DG increases its proportion of reactive power it injects to the system.

Consider the power response of each test case for load profile 1 in Fig. 5-27 overlaid on the generator capability plot. For the load levelling and parallel DG test of load profile 1 the power is within the capability limits defined by the manufacturer. This was also the case for profiles 2 to 5. The capability curve plots for the latter profiles are provided in Appendix S. The DG power while in the power reserve mode, shown in Fig. 5-27, did exceed the prime mover limit for a short period. This was represented by a 107% overload. Although undesirable, this is acceptable as it is permissible for engines to run at a maximum of 110% of their output power rating (Daffey and Hodge, 2004; Lloyd’s Register, 2017). The second factor that needs to be considered is that the generator is at its stator current limit, as shown by point B in Fig. 5-27. Point B is at the intercept of the thermal limit of the stator, which is a factor of the rated current of the machine. This is within the overload capability of the generator which can operate at 10% above rated current for one hour in every six hours (AvK-Alternators, 2014). Fig. 5-27 shows that the power factor of the DGs in the power reserve and parallel test case are maintained at near constant 0.85 PF lagging. Whereas, the load levelling response shown by point A, varies from 0.8 up to 0.95 PF lagging.
An interesting phenomenon was observed when the ESS charging power was in excess of 350 kW when the ESS was in load levelling mode. Oscillations occur in the VSC reactive power injected to the power system, which was reflected in the voltage, real power and frequency for the system response in profiles 2, 3 and 5. These are indications of system instability. The largest voltage deviation was exhibited in the results of load profiles 3 and 5, at less than ±1.5%, these were within the NATO STANAG requirement of ± 5% during steady state conditions. The root cause of the voltage instabilities during the power system load transients is likely to be a combination of the control interaction between the AVR and VSC control system, and the unequal output impedances of the power sources.

The primary layer of the VSC control system uses a control structure that is grid-supporting with multiple inner control loops (voltage and current loops) as detailed in section 4.7. Multi-loop control systems such as the one used here have increased sensitivity to impedance related stability compared to single loop controls, as discussed by Paquette and Divan (2014). The power sources have inherently different output impedance characteristics under transient loading conditions. Under transient loads the generator impedance is largely governed by the direct axis transient reactance, conversely VSC impedance is defined by the LCL filter characteristics. The difference in output impedance means that the voltage drop at the output of each source is unequal, and therefore the accuracy of reactive power load sharing is affected. This is because the voltage errors in the feedback loops of the power source under transient load conditions will be different, and consequently the dynamics of adjustments made by the AVR and VSC control system.

The VSC and generator have similar methods for voltage regulation, however the voltage regulation of the VSC is much faster than that of the AVR of the generator. Therefore the VSC inverter operates on a dynamic voltage droop, whereas the generator only operates in a linear voltage droop once the voltage reference error has been driven to zero by the AVR action. The small errors in the voltage set points create the circulating currents, which results in the voltage and reactive power oscillations exhibited in the load profiles simulated using modelling tool 2. Reactive power and voltage oscillations with similar control schemes have been exhibited in the work of Krishnamurthy et al. (2008) when modelling changes in system load, and during experimental testing of transient load sharing between a synchronous generator and VSC in Paquette et al. (2014), caused by circulating reactive current. In both works voltage oscillations of
approximately 1% were exhibited in their simulations and experimental results respectively, in the simulations of load profiles 1 through 5 the oscillation magnitude varied from 1.5% to 3%. This oscillation characteristic is comparable to Paquette et al. (2014) and Krishnamurthy et al. (2008) and therefore demonstrates that the behaviour simulated could be exhibited in a practical implementation of the system in modelling tool 2. The oscillations in this work are larger due to the VSC multi-loop control system, single loop control systems are implemented in Paquette et al. (2014) and Krishnamurthy et al. (2008).

The frequency oscillation magnitude during load transients is smaller in magnitude at 0.2% of rated system frequency. This is due to the fundamental differences in the frequency response of each power source. As detailed in section 2.3.2, the frequency of the generator voltage is controlled by adjusting the fuel flow to the diesel engine to control the speed in relation to the load torque. The speed reference is adjusted in proportion to the measured real power. Conversely the VSC directly outputs a voltage frequency proportional to the measured real power at a faster rate, acting as a stiff frequency source and therefore dominates the generator’s transient behaviour. Consequently, during a load transient the generator will see a relatively small speed error and will adjust the mechanical input power of the engine relatively slowly.

In assessing the QPS response of the system when subjected to load profiles 1 to 5, the results have highlighted that the reactive power sharing ability of the system when simulated using modelling tool 2 does not respond as intended. There is also an element of coupling of the real and reactive power, which is exhibited in Fig. 5-20 (a), Fig. 5-21 (a) and Fig. 5-24 (a). To investigate the reactive power sharing and therefore voltage response of the system further and to see whether the oscillations could be reduced, a droop sensitivity study was conducted. This is presented in the following sub-section.

5.5.4 Exploring the impact of varying VSC droop

Ship power systems, unlike land-based microgrids, are characteristically weak due to the comparatively limited inertia of the prime movers. This subsection investigates how the system responds when the VSC voltage-reactive power droop is modified. Fig. 5-28 shows the droop characteristics applied in the sensitivity study. These correspond to a droop range of 1 to 5%. In section 4.11.2, the VSC droop setting selected was 18.5 V/MVAr, 5% voltage droop. This droop setting was selected because the maximum permanent voltage variation permitted by NATO STANAG 1008 is 5% of rated voltage.

![Fig. 5-28. Q-V droop sensitivity study of $n_{VSC}$](image)
The results sought in the sensitivity study are the reactive power sharing response and the voltage response, for both load changes and recharging the ESS. The load profile used in the sensitivity study is presented in Fig. 5-29. The initial load is 2.4 MW 0.85 PF lagging and ramps up to 2.9 MW, prior to ramping down to 1.9 MW. The final load ramp returns the load to 2.4 MW. Analysis of the system response has been compartmentalised into five key phases, identified in Fig. 5-29, which are as follows:

1) Synchronisation, $8 < t < 20$ s. (criteria for synchronisation is detailed in section 4.11.1)
2) Load ramp up, $25 < t < 40$ s.
3) Load ramp down I, $50 < t < 65$ s.
4) Load ramp down II, $70 < t < 80$ s.
5) Recharge and load ramp up, $80 < t < 110$ s.

![Fig. 5-29. Droop sensitivity load profile applied to modelling tool 2](image)

1) Synchronisation phase

Fig. 5-30 shows the reactive power and voltage response during the synchronisation phase. Prior to synchronisation, when $t < 10$ s the DG and VSC are supplying 1.5 and 0.03 MVAR respectively, as shown in Fig. 5-30(b) and (c). At the moment of synchronisation, as previously reported in section 5.5.1, the reactive power of the DG decreases, whilst the VSC reactive power output increases. Reducing the gradient of $p_{VSC}$ causes the VSC to deliver more of the reactive power allocation to the load, whilst the DG reduces its share. The bus voltage in Fig. 5-30 (d), prior to synchronisation is 687 Vrms. After synchronisation the additional power source increases the bus voltage to 689 Vrms after the initial voltage deviation.
Fig. 5-30. (a) Load demand (b) $Q_{DG}$ (c) $Q_{ESS}$ and (d) bus rms voltage response during synchronisation phase

2) Load ramp up phase

Fig. 5-31 shows the system response during the load ramp up phase to 2.9 MW at 0.85 PF. $Q_{DG}$ increases to meet the load demand, while the $Q_{ESS}$ reduces, absorbing reactive power during the load ramp up, as shown in Fig. 5-31 (b) and (c). With reduced $n_{VSC}$ the DG’s rate of response to satisfying the reactive power demand is more pronounced and immediate. Consequently, the magnitude of the drop in $Q_{ESS}$ is largest, as is the voltage drop on the main bus, as shown in Fig. 5-31 (d).
Power reserve and load levelling modes

3) Load ramp down phase I

During the ramping down of the load, the VSC injects the greatest amount of reactive power when $n_{VSC}$ is at its lowest setting as shown in Fig. 5-32 (b) and (c). Consequently, the larger bus voltage deviation occurs when $n_{VSC}$ is 3.7 V/MVAR.

Fig. 5-32. (a) Power demand (b) $Q_{DG}$ (c) $Q_{ESS}$ and (d) bus rms voltage response during load ramp down phase I
4) Load ramp down phase II

A similar phenomena to that shown in Fig. 5-31 is observed in the second load ramp down stage in Fig. 5-33, where $Q_{DG}$ is at its minimum when $n_{VSC}$ is at its minimum value, and the voltage deviation is at its maximum.

![Graphs showing power demand and voltage response during load ramp down phase II](image)

Fig. 5-33. (a) Power demand (b) $Q_{DG}$ (c) $Q_{ESS}$ and (d) bus rms voltage response during load ramp down phase II

5) Recharge and ramp up phase

In section 5.5.1 it was shown that during recharge of the ESS, oscillations occur in reactive power between the DG and VSC, the voltage on the main bus also oscillates in accordance with the reactive power oscillations. Fig. 5-34 shows the system response during the recharge and ramp up phase of the load profile in Fig. 5-29. During this phase the magnitude of the voltage oscillation is at its lowest when $n_{VSC}$ is 3.7 V/MVAr. This would be expected, as the reduced droop coefficient results in increased stiffness of the power system.
A summary of the voltage response characteristics for load change phases of the load profile is provided in Table 5-13. From Table 5-13 it can be deduced that, with increasing value of $n_{\text{VSC}}$ the voltage deviation during load ramp up and ramp down decreases. Whereas, the magnitude of the oscillations in voltage response decreases with decrease in value of $n_{\text{VSC}}$. All deviations recorded in the droop sensitivity study are within NATO STANAG 1008 limits for steady state deviation. Based upon the results in Table 5-13, it is recommended that the droop coefficient be maintained at less than 2%. This reduces the magnitude of the voltage deviation during phase 5, there the deviations are higher than during phases 2 through 4.

Table 5-13. Summary of droop sensitivity results

<table>
<thead>
<tr>
<th>Phase</th>
<th>2) load ramp up</th>
<th>3) load ramp down I</th>
<th>4) load ramp down II</th>
<th>5) recharge and ramp up</th>
</tr>
</thead>
<tbody>
<tr>
<td>$n$ referred to 690 V (V/MVAR)</td>
<td>Voltage drop</td>
<td>Voltage rise</td>
<td>Voltage rise</td>
<td>Max voltage</td>
</tr>
<tr>
<td>3.7</td>
<td>-1.75%</td>
<td>1.74%</td>
<td>1.62%</td>
<td>2.38%</td>
</tr>
<tr>
<td>7.4</td>
<td>-1.61%</td>
<td>1.63%</td>
<td>1.54%</td>
<td>2.39%</td>
</tr>
<tr>
<td>11.1</td>
<td>-1.52%</td>
<td>1.51%</td>
<td>1.49%</td>
<td>2.70%</td>
</tr>
<tr>
<td>14.8</td>
<td>-1.44%</td>
<td>1.45%</td>
<td>1.45%</td>
<td>4.06%</td>
</tr>
<tr>
<td>18.5</td>
<td>-1.35%</td>
<td>1.37%</td>
<td>1.38%</td>
<td>3.59%</td>
</tr>
</tbody>
</table>
5.5.5 Fuel savings

This sub-section of the results assesses the fuel consumption of the DG when the ESS is in load levelling mode and compares these results with the cases of two DGs operating in parallel, and one DG operating with the ESS in power reserve mode. Fig. 5-35(a) presents the energy delivered \( E_{\text{delivered}} \) during each load profile in load levelling mode, calculated using (5.4). This is plotted as a function of the percentage fuel savings against the case of two parallel DGs. Fig. 5-35(b) presents the comparison of the load levelling fuel savings compared to when the ESS is in power reserve mode. The simulated battery ESS SoC for each load profile is shown in Fig. 5-36. For each simulation, the initial SoC, \( SoC_0 \), of the ESS begins at 70% SoC. Full fuel performance results are provided in Appendix T.

\[
E_{\text{delivered}} = E_{\text{rated}} (SoC_0 - SoC_{\text{end}})
\]  

(5.4)

where \( SoC_{\text{end}} \) is the final recorded SoC of the ESS at the end of the simulation.

![Fig. 5-35. Energy delivered by the battery ESS in load levelling mode as a function of fuel savings compared to (a) 2 DGs and (b) 1 DG with ESS in power reserve mode](image)

![Fig. 5-36. SoC of the battery ESS for each load profile simulated (see profiles 1-5 in Fig. 5-17)](image)
Profile 1 demonstrated the highest fuel savings during load levelling at 22.9% (15.8 kg) compared to the parallel DG operation. Profile 3 exhibited 22.6% (15.6 kg) fuel savings. During power reserve operating mode, profile 3 exhibited the highest fuel savings of 2.03% (1.1 kg), compared to load levelling operation. Profile 1 was slightly lower for the power reserve case due to the near constant nature of the load and the time spent at higher power in load profile 3 compared to load profile 1. As expected, the more energy delivered by the ESS, the higher the fuel savings. This correlation can be identified in Fig. 5-35(a) and (b) by considering the two groupings of fuel savings results for profiles 2, 4 and 5, and profiles 1 and 3.

### 5.5.6 Exhaust GHG emissions

Table 5-14 details the reduction in total GHG emissions when the ESS is in load levelling and power reserve modes. Full results are provided in Table T-1, Table T-3 and Table T-4 in Appendix T. The exhaust GHG emissions when the ESS is in load levelling mode are constant due to the near constant power output from the DG operating at 85% MCR. Conversely, the fuel consumed and thus exhaust GHG emissions emitted in the parallel DG mode and when the ESS is in power reserve mode is variable. Importantly the GHG emissions reduction is a minimum of 34.15% when the ESS is installed compared to conventional operation with parallel DGs. Due to the DG having to manage the variability of the load when the ESS is in power reserve mode, the emissions are higher for all load profiles compared to when the DG is held at the constant 85% MCR during load levelling mode.

<table>
<thead>
<tr>
<th>Profile</th>
<th>Total polluted GHG exhaust emissions (kg)</th>
<th>Emissions saved vs parallel DG operation</th>
<th>Delta between load levelling and power reserve</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td>Load levelling mode</td>
<td>Power reserve mode</td>
</tr>
<tr>
<td>1</td>
<td>264.5</td>
<td>36.32%</td>
<td>34.72%</td>
</tr>
<tr>
<td>2</td>
<td>263.7</td>
<td>36.12%</td>
<td>35.32%</td>
</tr>
<tr>
<td>3</td>
<td>263.9</td>
<td>36.17%</td>
<td>34.15%</td>
</tr>
<tr>
<td>4</td>
<td>263.8</td>
<td>36.15%</td>
<td>35.31%</td>
</tr>
<tr>
<td>5</td>
<td>263.2</td>
<td>36.00%</td>
<td>35.24%</td>
</tr>
</tbody>
</table>

### 5.6 Implications and consequences

The secondary operating modes of the battery ESS are power reserve, and load levelling. Both operating modes were investigated in this chapter using steady state and quasi-steady state simulation investigations respectively for the candidate warship power system. First, the implications of the power reserve investigation results are discussed, prior to the load levelling investigation results.
5.6.1 Power reserve investigation

This investigation explored the research problem of quantifying the power system performance when the ESS operates in power reserve mode as described in section 3.7.

As discussed in section 3.5.3, the loading margins of the generators can be increased by using the power reserve of the ESS to provide power to the ships consumers in the event of the DG that is online and supplying load becoming inoperable. In this investigation the number of failed generator sets was assumed as one when quantifying the power system performance when the ESS is operating in power reserve mode. The first implication is that the minimum allowable SoC of the ESS to facilitate power supply to the ship if a generator goes offline was determined to be 50% SoC.

When the candidate ESS is 50% SoC or above, it was shown by the results presented in Table 5-6 that the power available from the ESS on each switchboard is, in the worst case, 2.34 MW for a 3 minute ride through event. Consequently, the implications that can be drawn from the perspective of fuel consumption of the candidate electric power system are:

1) The generator loading margins are increased. In closed bus condition, the loading margins are 100%, and a minimum of 80% in the split bus condition as shown in Fig. 5-5. This results in a higher fuel efficiency condition compared to conventional operating practice without ESS.

2) Table 5-7 shows that the number of generator sets running in each operating state, except RAS and State 1 can be reduced due to the increased generator loading margins. This results in engine fuel, overall running hours and GHG emissions savings against the baseline candidate warship power system.

The CAPEX of the battery ESS and integration interface to the candidate warship power system was analysed in section 5.3.3. The aim of this analysis was to quantify the payback period of the CAPEX during operation of the ESS in power reserve mode. The analysis also considered the maximum number of cycles per year to ensure that the ESS CAPEX cost did not outweigh the OPEX savings before a replenishment of the batteries is required. The range of payback period based on the analysis presented is 1.3 to 5.4 years.

These figures were based on maximum fuel cost and minimum predicted CAPEX, and lowest fuel cost and maximum CAPEX respectively for the ASW profile.

A summary of the key implications of the results generated in the power reserve investigation is offered in Table 5-15. The consequences that relate to the implications are also presented.
Table 5-15. Summary of the key implications and consequences resulting from using the candidate battery ESS in the power reserve mode in warship power systems

<table>
<thead>
<tr>
<th>Implication</th>
<th>Consequence</th>
</tr>
</thead>
<tbody>
<tr>
<td>The minimum SoC of the ESS in power reserve mode when providing power for 3 minutes until another DG is online is 50% SoC.</td>
<td>If the ESS is in power reserve mode and the SoC is less than 50% at commencement of providing power reserve when a generator unexpectedly goes offline for 3 minutes, the ESS could surpass the 20% SoC limit. Discharging to deep depths of discharge can cause lithium plating, which can result in capacity fade and internal resistance increase (Birkl et al., 2017; Jalkanen et al., 2015).</td>
</tr>
<tr>
<td>The power rating of the ESS is sufficient to increase the loading margins of the generator sets to 80% in split bus operating configuration. During closed bus the generator loading margins are 100%.</td>
<td>Reduces the time that the ships generators will spend at part load operation, resulting in higher fuel efficiency over the ships operating profile. This could reduce the centrifugal forces in the engine and engine wear compared to running at rated speed and part load (Geertmsa, 2019). Furthermore, the ignition delay during combustion of diesel engines running at low load could be avoided, reducing NOx emissions (Brückner et al., 2018). Running at higher load reduces the fuel consumption and GHG exhaust emissions. This could result in an extended range of the candidate ship (depending upon other variables such as food stores) or reduced volume requirements for the fuel tanks.</td>
</tr>
<tr>
<td>The minimum exhaust GHG emission reduction is under 40% compared to conventional generator operating configurations.</td>
<td>The use of the ESS specified in this research to operate in power reserve mode significantly reduces the ship’s exhaust GHG emissions footprint. The reduction represents a significant proportion of the 50% GHG emissions reduction target by 2050 set by the IMO MEPC (2018).</td>
</tr>
<tr>
<td>The power rating of the ESS is sufficient to reduce the number of running generator sets in all operating states and climate conditions, except the RAS and State 1 operating states.</td>
<td>Reduction in the number running hours of the generator could increase the MTBO.</td>
</tr>
</tbody>
</table>

5.6.2 Load levelling investigation

The load levelling investigation explored the research problem of power system QPS and fuel consumption when load sharing occurs between a DG and ESS during quasi-steady state conditions for the candidate power system.

As discussed in section 3.7.1, NATO STANAG 1008 is recognised as the baseline QPS standard for AC low voltage power systems. In the load levelling investigation, QPS was investigated in the context of stability of voltage and frequency against the steady state tolerance limits, defined by the standard as being ± 5% and ±3% respectively. In investigating the level of adherence to NATO STANAG 1008 the following implications can be drawn from a QPS perspective when using the candidate ESS to load level a DG in the candidate hybrid power system. Note this applies to when the VSC is controlled as a grid-supporting converter with conventional droop control:
1) The results in section 5.5.1, Chapter 5 and 5.5.4 demonstrate that the power sources do not directly follow the reactive power set point when sharing reactive power of the load between the DG and VSC. It is argued that this is a consequence of the power source impedance characteristics and control system interaction.

2) The results in Fig. 5-26 show that deviations from the system rated voltage are of greater magnitude when using the DG and VSC when the ESS is in load levelling mode compared to DG only operation.

The second part of the load levelling investigation was applied to analysis of the fuel consumption and GHG exhaust emissions performance. This was in order to identify any possible savings that could be available in fuel consumption and the associated reduction in exhaust GHG emissions from the candidate warship against current practice. The implications that can be drawn from this part of the load levelling investigation are:

1) The results in Fig. 5-35 show that when the ESS is operating in load levelling mode, fuel consumption can be reduced up to 22.9% compared to conventional parallel DG operation for the load profiles tested in this investigation.

2) When the ESS is operating in power reserve mode, fuel consumption can be reduced up to 21.8% compared to conventional parallel DG operation for the load profiles tested in this investigation.

3) The results in Table 5-14 show that the ESS in load levelling mode reduces GHG exhaust emissions by up to 36.3% compared to conventional parallel DG operation.

4) The results in Table 5-14 show that the ESS in power reserve mode reduces GHG exhaust emissions by up to 35.3% compared to conventional parallel DG operation.

A summary of the key implications of the results generated in the load levelling investigation is offered in Table 5-16. The consequences that relate to these implications are also presented. Recommendations relating to the consequences of using the candidate ESS in the power reserve and load levelling operating modes in warship power systems is provided in the following section and concludes this chapter.
Table 5-16. Summary of the key implications and consequences resulting from using the candidate battery ESS in the load levelling mode in warship power systems

<table>
<thead>
<tr>
<th>Implication</th>
<th>Consequence</th>
</tr>
</thead>
<tbody>
<tr>
<td>The DG and VSC do not directly follow the reactive power set point when sharing reactive power of the load.</td>
<td>Circulating reactive power in the power system in extreme circumstances could result in overcurrent damage of the VSC if the circulating currents are large (Yao et al., 2011).</td>
</tr>
<tr>
<td>Deviations from the system rated voltage are higher in magnitude when using the DG and VSC when the ESS is in load levelling mode compared to DG only operation.</td>
<td>System deviations in voltage under quasi-steady state conditions are within NATO STANAG 1008 limits. Under extreme situations, large circulating currents could lead to power system instability (Han et al., 2017).</td>
</tr>
<tr>
<td>When the ESS is in load levelling mode, fuel consumption can be reduced up to 22.9% compared to conventional parallel DG operation for the load profiles tested in this investigation. When in power reserve mode the fuel consumption can be reduced by up to 21.8%.</td>
<td>The fuel consumption saving could benefit the candidate warship by either reducing the requirement for tank volume or extend the range by extending the time required between refuelling. From a naval architecture perspective, reducing the tankage volume requirement could allow more tank volume for other liquids such as aviation fuel, increase deck heights or impact the design of the hull form, depending on design maturity.</td>
</tr>
<tr>
<td>When the ESS is in load levelling mode exhaust GHG emissions are reduced by up to 36.3% compared to conventional parallel DG operation. In power reserve mode emissions are reduced by up to 35.3%.</td>
<td>Load levelling and power reserve modes of the ESS reduces the environmental impact of exhaust GHG emissions from the candidate warship.</td>
</tr>
</tbody>
</table>

5.7 Summary and system recommendations

The aim of this chapter was to investigate the secondary modes of operation of the candidate battery ESS in the candidate warship power system. The operating modes investigated were power reserve and load levelling using modelling tool 1 and tool 2 respectively. Each tool was detailed in chapter 4. Section 5.2 presented the justification for, and description of the power reserve investigation. The results when the ESS is operating in power reserve mode, over the operating profiles of the candidate warship, were presented in section 5.3, with and without the ESS integrated with the power system. The second investigation was justified and detailed in section Chapter 5, the results were presented and discussed in section 5.5. The second investigation compared the performance of the load levelling operating mode with conventional parallel DG operation and when the ESS is operating in power reserve mode. The implications and consequences of both investigations were offered in section 5.6. Using the two-part investigation in this chapter the following sub-section concludes the chapter with recommendations that this research has contributed to the field when using a battery ESS in power reserve, and load levelling modes in warship electric power systems. The analysis of the ability of the battery ESS to power the LDEW in the ESS primary operation mode using modelling tool 3 is presented in chapter 6.
5.7.1 Recommendations

The recommendations in this sub-section are considered from the perspective of the naval design authority responsible for integrating the candidate ESS and ESS interface with the warship power system.

With regard to the ESS acting as a power reserve, it is recommended that the ESS is always maintained above 50% SoC when in power reserve mode. This is to prevent lithium plating, which can result in capacity fade and internal resistance increase of the cells in the ESS.

With regard to the ESS operating to load level a DG, it is recommended that the droop coefficient be maintained at less than 2%. This is to limit the magnitude of the voltage deviation on the main distribution bus.
Chapter 6 LDEW power supply mode

6.1 Introduction

To address the research problems for a battery ESS performing the function of LDEW power supply as identified in section 3.7, it was proffered that time domain simulation techniques be used to investigate the impact of LDEW loading on the Li-ion NMC battery ESS and the system dynamics. As discussed in section 3.7, such investigation is required because the transient response and operating envelope of this particular battery type when under LDEW loading is not yet fully understood. Modelling tool 3, the development of which was detailed in chapter 4, comprises a dynamic model of the battery ESS, DC/DC boost converter and LDEW load. This was constructed in MATLAB/Simulink. This modelling tool is capable of simulating the transient response to LDEW load conditions when using the control system developed in section 4.12. The requirement is for LDEWs to be supplied to deliver up to four minutes of operation (Markle, 2018). The impacts of this requirement on the candidate battery ESS and system transient response have been previously unknown. Albeit some issues are expected such as limited useable SoC range under LDEW loading as a consequence of the large current drawn by the LDEW load, which could cause a significant drop in the ESS terminal voltage capable of surpassing the voltage cut-off limit, as previously identified in section 3.5.2. Hence, this chapter will present the results of simulation based research into the ability of the battery ESS to supply power to LDEW loads for the four minute requirement. As discussed in section 2.4.2, cells in a battery based ESS degrade with calendar and cyclic aging, which could impair the ability of the battery ESS to operate as an LDEW power supply, therefore both the BoL and degraded performance of the battery ESS are investigated in this chapter.

This chapter is divided into the following five main parts, which focus on the ability of the battery ESS to power LDEW loads in the candidate warship power system.

1) The first part describes the LDEW power supply investigation and justifies the need to explore the capability of the candidate battery ESS, selected in chapter 3, to power LDEWs.

2) The second part presents an investigation into the battery ESS powering the LDEW when at BoL conditions. This part considers the system’s dynamic response, battery operating envelope, thermal loss and recharge implications.
3) Having set the system performance benchmark when the ESS is at BoL, the third part of this chapter investigates how the degraded conditions of the ESS could impact system performance when conducting LDEW operations. Degraded conditions refer to either capacity fade or internal resistance increase of the cells in the battery ESS as a consequence of cyclic and/or calendar aging, as detailed in section 2.4.3.

4) The fourth part of the chapter discusses the implications and consequences of the results presented in the previous two sections.

5) Following the discussion of implications and consequences offered from the two investigations, this chapter concludes with recommendations to the naval design authority and battery ESS designer when consideration is being given to the use of a battery ESS of the design and functionality as that of the candidate ESS to power LDEWs.

6.2 LDEW power supply investigation

6.2.1 Investigation justification

The purpose of this investigation is to explore the capability of the candidate battery ESS selected in chapter 3 to act as the power source for predicted high power LDEW loads that are highly dynamic with relatively short rise times. From the power system perspective, the justification for using a battery ESS for this operating mode is to mitigate against the adverse QPS conditions that could arise from powering the LDEW directly from DGs via a power converter. In the context of this investigation, adverse QPS conditions refers to when the LDEW bus voltage is outside of the ±10% tolerance as required by IEEE 1709 (IEEE Industry Applications Society, 2018). From the DG perspective, using the battery ESS to power the LDEW alleviates the potential thermal and mechanical stresses of loading and unloading the DGs at high ramp rates and, for a DG, relatively high power cycling frequency that could be detrimental to engine life and performance. As discussed in section 2.4.2, the performance of the cells in a battery ESS degrade through a combination of cyclic and/or calendar aging related mechanisms. As such, this investigation is considered in two parts. First, to reflect the BoL performance. Second, the degraded performance under LDEW loading conditions. In both parts of the investigation, the aim is to investigate the research problems described previously in section 3.7; these being:

1) To explore and understand the battery ESS and DC/DC power converter dynamic response to LDEW pulsed power loads from the perspective of the ESS and system QPS.

2) To explore the capability of the candidate Li-ion based NMC battery ESS to supply LDEW loads to satisfy the power and energy demand for a sustained four minute engagement requirement.

3) To explore the extent to which the useable SoC range of the candidate battery ESS is reduced by operating LDEW loads.

4) To explore the system cooling implications of operating the battery ESS and DC/DC converter under LDEW load conditions.
5) To explore the implications of recharging the ESS following an LDEW operation on the candidate warship power system.

The justification for conducting this investigation is to obtain results that demonstrate the capability of the candidate Li-ion based NMC battery ESS selected in section 3.4.1, to power LDEW loads and the constraints that this places on system design in terms of meeting QPS criteria and thermal losses. The results of this investigation will then be used to support and develop recommendations for power system designers on the operating envelope of this particular battery ESS to provide power to LDEW loads via a DC/DC boost converter in warship power systems.

Exploring the dynamic response and QPS when the candidate battery ESS is required to provide power to the LDEW will inform system designers as to the limitations of using the candidate battery ESS and a single stage DC/DC converter, as selected for investigation in this research. It is important to identify the voltage deviations that other components connected to the LDEW output bus may be exposed to under LDEW operation. Knowledge of any QPS implications of LDEW loading is also relevant to LDEW designers as the time to full radiant intensity will influence the converter dynamics and therefore the voltage deviation on the output LDEW bus. The power and time to full radiant intensity of the LDEW load will have a transient impact on the battery and converter control system response, which could have consequential impact on voltage QPS. It is therefore of importance to explore the transient response of the candidate battery ESS and control system to LDEW loading.

For both investigations in this chapter, one of the aims is to determine the minimum initial SoC of the battery ESS required to deliver a four minute LDEW engagement. The justification for this is to inform the operator of the operating constraints that must be adhered to such that the battery ESS does not surpass its cut-off voltage and cause damage to the cells in the battery ESS.

Exploring the implications on thermal load of the battery ESS and DC/DC converter provides important information to the designers of the battery ESS and the thermal load that needs to be managed by the system level cooling. This is important for battery ESS design to allow thermal stresses to be mitigated during the design phase of the battery modules and strings. From a cell perspective, thermal stress is a significant cause of degradation mechanisms including SEI layer growth and electrolyte decomposition, which can cause capacity fade in Li-ion cells. The results of this exploration can then be used in future work when the thermal proximity effects of cells charging and discharging are considered in detailed ESS design.

For both the BoL and degraded investigations, modelling tool 3, detailed in chapter 4, is used. This is shown in Fig. 6-1, which is a simplified representation of modelling tool 3. The actual model schematic is provided in Appendix L. The modelling assumptions and limitations were detailed in section 4.4.3. To determine the losses of the battery ESS during discharge to the LDEW and during charging of the battery ESS after LDEW operation, the efficiency maps used in modelling tool 1 were integrated with modelling tool 3.

To conduct the investigation the time to full radiant intensity and peak pulse power of the LDEW is adjusted using the variable load model described in chapter 4. Reducing the time to full radiant intensity of the LDEW increases the ramp rate demand on the battery ESS and response of the DC/DC converter control
system. The converter control system controls the IGBT switching device duty cycle using current mode control, which controls the inductor current, thereby the battery discharge current and the output voltage as shown in Fig. 6-1. This process was described in sections 3.6.1 and 4.12.2. Adjusting the time to full radiant intensity will therefore impact on the output voltage QPS. Different initial settings of the battery SoC allows the simulation of adjusting the available energy that can be supplied to the weapon, from which the impact on the operating envelope of the battery ESS can be explored.

![Diagram of system](image)

**Fig. 6-1.** Simplified circuit representation of the system in modelling tool 3 for BoL and degraded condition investigations

A summary of the tests undertaken in each part of the investigation and the results pursued is presented in Table 6-1. A detailed description of the tests undertaken for BoL and degraded conditions is provided in sections 6.3 and 0 respectively.
Table 6-1. Summary of research tests and results pursued for LDEW investigation

<table>
<thead>
<tr>
<th>Investigation</th>
<th>Test</th>
<th>Description</th>
<th>Results sought</th>
</tr>
</thead>
<tbody>
<tr>
<td><strong>Beginning of life</strong></td>
<td>Time to full radiant intensity, ( T_r = 25 \text{ ms}, 50 \text{ ms and 100 ms} )</td>
<td>Simulate the LDEW firing cycle until the battery ESS reaches cut-off voltage or 10 minute simulation limit. Simulate LDEW firing with time to full radiant intensity at 25 ms, 50 ms and 100 ms. Repeat for peak pulse power between 1.5 MW to 2 MW at 0.25 MW increments. Repeat for battery SoC between 50% and 90%, at 10% increments.</td>
<td>Output voltage (V)</td>
</tr>
<tr>
<td></td>
<td>Peak pulse load power 1.5 MW ≤ ( P_{LDEW} ) ≤ 2 MW (0.25 MW increments)</td>
<td>Apply degradation conditions to the battery ESS by increasing the internal resistance and decreasing the useable capacity against the matrix in Table 6-8. Simulate four-minute LDEW firing cycle to identify the minimum allowable SoC of the battery to complete the four-minute firing cycle in each degraded condition. Simulate for each peak LDEW power.</td>
<td>Output voltage (V)</td>
</tr>
<tr>
<td></td>
<td>Battery SoC 50% ≤ SoC ≤ 90% SoC (10% increments)</td>
<td></td>
<td>Output voltage deviation (%)</td>
</tr>
<tr>
<td><strong>Degraded</strong></td>
<td>Time to full radiant intensity, ( T_r = 25 \text{ ms} )</td>
<td></td>
<td>Output power response (W)</td>
</tr>
<tr>
<td></td>
<td>Peak pulse load power 1.5 MW ≤ ( P_{LDEW} ) ≤ 2 MW (0.25 MW increments)</td>
<td></td>
<td>Battery voltage (V)</td>
</tr>
<tr>
<td></td>
<td>Resistance increase and capacity decrease (see Table 6-8)</td>
<td></td>
<td>Battery current (A)</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td>Battery SoC (%)</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td>Number of shots to battery cut-off voltage (#)</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td>Battery FR losses (MJ)</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td>Mean battery and converter heat loss (W)</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td>Peak battery and converter heat loss (W)</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td>Recharge time (s)</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td>Recharge power (W)</td>
</tr>
</tbody>
</table>

### 6.3 Beginning of life system investigation

This section will present the results of research conducted into the impact of LDEW loading on the performance of the battery ESS and converter dynamics when the battery ESS is at BoL conditions. The aim of this section is to determine the SoC range within which the battery ESS can meet the four minute LDEW engagement time requirement (specified by Markle (2018)) for LDEW peak pulse power and time to full radiant intensity before the voltage cut-off is breached for different settings of time to full radiant intensity. Following this, examination of the implications of the system response on output voltage QPS will be presented. The wider system implications of recharging the battery ESS following an engagement, and the I^R losses from the battery ESS and converter losses during LDEW firing are then explored.

#### 6.3.1 Investigation description

This first study assessed system performance with the battery ESS under non degraded, BoL conditions and the results are presented in Fig. 6-1 for the LDEW parameters detailed in Table 6-2. As discussed in chapter 3, there is an inherent delay prior to achieving full radiant intensity of the laser aperture, which is in the tens of milliseconds (Titterton, 2015). This delay is captured in this investigation as the rise and fall time, \( T_r \) and \( T_f \), of the LDEW load. \( T_r \) is assumed as equal to \( T_p \), where \( T_r \) is adjusted from 25 ms to 100 ms. The pulse duration, \( T_{on} \) of the LDEW load is 2.5 s, as used in Mills et al. (2018). This concurs with Moran
(2012) and McAulay (2011) who both conclude that $T_{on}$ is normally in the order of seconds. $R_P$ is controlled to achieve a trapezoidal shaped pulse equivalent to Fig. 3-15, which is defined by $P_{LDEW}$, $T_r$ and $T_{on}$ as detailed in Table 6-2.

For each LDEW load, simulations were conducted at 10% intervals between 90% and 50% initial SoC of the battery ESS, for $T_r$ of 100 ms, 50 ms and 25 ms. Intervals were set at 10% to assess the battery operating envelope, while limiting the computational cost of simulations at shorter SoC intervals. LDEW loading in the simulations commence at 5 s to ensure the system had attained a steady state condition. Each simulation was prescribed to terminate if either the 800 V battery ESS cut-off, 3,072 A discharge current, 20% SoC or 10 minute simulation limit were surpassed. The battery is limited to operate between 90% and 20% SoC to mitigate against cell degradation (Hannan et al., 2018).

Table 6-2. LDEW characteristics for BoL system investigation

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
<th>Unit</th>
</tr>
</thead>
<tbody>
<tr>
<td>LDEW peak power demand after losses, $P_{LDEW}$</td>
<td>2, 1.75, 1.5</td>
<td>MW</td>
</tr>
<tr>
<td>Rise time/fall time, $T_r/T_f$</td>
<td>100, 50, 25</td>
<td>ms</td>
</tr>
<tr>
<td>Pulse duration, $T_{on}$</td>
<td>2.5</td>
<td>s</td>
</tr>
<tr>
<td>Duty cycle</td>
<td>40</td>
<td>%</td>
</tr>
<tr>
<td>Base load</td>
<td>50</td>
<td>kW</td>
</tr>
</tbody>
</table>

6.3.2 System response to 2 MW LDEW load profile

This section aims to demonstrate how the system responds to the LDEW load profile before further exploration of the research problem is conducted. To investigate the impact of LDEW firing on the system dynamics, the system in Fig. 6-1 was first simulated when the battery is subjected to providing a 2 MW LDEW peak power profile with $T_r$ of 25 ms, when the battery ESS is at 90% SoC at the start of the simulation. When the battery is at 90% SoC, prior to firing an LDEW pulse, the input voltage to the DC/DC converter is at its maximum as determined by the maximum allowable SoC limit in this investigation. It is at this SoC condition where the voltage delta between the input and output voltage of the converter will be at its minimum, therefore, the output voltage deviation should be at its lowest under LDEW loading. Further, the available energy in the battery ESS is at its maximum and the voltage delta between the operating voltage of the ESS and the cut-off voltage is at its maximum, therefore the number of shots available at peak $P_{LDEW}$ should be at its maximum.

Fig. 6-2 shows the first 60 s of operation. Fig. 6-2(a) shows that when the first 2 MW LDEW pulse is applied at 5 s, the voltage drop across the battery ESS terminals is 180 V, which is primarily a function of the internal resistance of the battery cells in the ESS. Fig. 6-2(b) shows the response of the output voltage, $v_o$ under the LDEW profile. For the initial 60 s of operation $v_o$ is maintained within transient limits as shown by the dashed lines. The largest drop and increase in $v_o$ are during the rise and fall time of the LDEW pulse respectively. Throughout the simulation, $i_{bess}$ is maintained below the 3,072 A battery ESS discharge limit as shown in Fig. 6-2(c). The response of $i_{bess}$ in Fig. 6-2(c) is similar to HIL testing results of a 650 V, 660 kW capable li-ion battery system presented by Langston et al. (2019), in Fig. 5(a) of the referenced
publication. The battery system in their work was subjected to a series of two-level periodic load pulses of 1 kA in magnitude. The paper did not discuss the limitations of this loading on the operating envelope of the battery, a factor that is explored in the investigation presented in this thesis and that consequently provides a novel contribution to the literature of battery performance under LDEW loading. The aim of their paper was to conduct commissioning, charge and discharge tests for expanded HIL testing.

Fig. 6-2. Simulated (a) battery voltage (b) converter output voltage, \( v_o \) (c) current and (d) output power response when the battery initial SoC = 90\%, \( P_{LDEW} \) = 2 MW and \( T_r \) = 25 ms.

Fig. 6-3 shows the remainder of the simulation results presented in Fig. 6-2. For this particular test condition the 800 V cut-off limit was breached at 407 s (65 shots), as shown by point A in Fig. 6-3(a). The final recorded SoC for the case in Fig. 6-3 was 68\%. This demonstrates that the battery ESS response to the extreme loading condition of the LDEW, from a voltage perspective, is sufficient to limit the useable SoC range under LDEW operations to 22\% of the total stored capacity in the ESS. Whilst cut-off voltage is often interpreted as an indication of low SoC, as discussed in section 3.5.2, the cut-off voltage termination criteria has been used as a protection measure that is representative of the actual battery protection system logic. The protection logic is in place to reduce the risk of degradation mechanisms such as lithium plating. Lithium plating potentially causes power and capacity fade of the battery system through loss of lithium or active material at the cell (Birkl et al., 2017; Jalkanen et al., 2015). The exploration of the limits on initial SoC and LDEW operation duration is presented in section 6.3.4.

The impact of the variable voltage characteristic of the battery ESS on output voltage QPS is shown in Fig. 6-3(a). The reducing voltage of the battery ESS over the course of the simulation increases the maximum
voltage deviation at the converter output terminals, until the transient tolerance is exceeded at 350 s and continues to exceed the transient tolerance until the simulation terminates, as highlighted by area B in Fig. 6-3(a). The reduction in battery voltage is not sufficient for the battery ESS to exceed the maximum discharge current over the duration of the simulation as shown in Fig. 6-3(b). This justifies the decision to require four battery strings for this investigation.

Fig. 6-3. Simulated (a) battery and output voltage response, and (b) battery and output current response when the battery initial SoC = 90%, $P_{LDEW} = 2$ MW and $T_r = 25$ ms.

Fig. 6-4 magnifies the system voltage response to the final LDEW pulse before the battery cut-off voltage is exceeded. Fig. 6-4(a) demonstrates that during the time to full radiant intensity, the output voltage drops until peak $P_{LDEW}$ is attained at 398.75 s. The output voltage then recovers to the 1,500 V set point within 0.2 s. Conversely, when $P_{LDEW}$ reduces to 0 MW between 401.25 and 401.50s, the output voltage increases as the battery is unloaded and the battery voltage recovers to 985 V. It is during this 25 ms when the inertia in the system from the passive devices causes overvoltage, and the peak of 1,654 V is attained as shown in Fig. 6-4(a). Following this peak, the output voltage recovers to 1,500 V within 0.5 s.

The peak voltage in this instance is marginally over the transient voltage tolerance as prescribed by IEEE 1709 (IEEE Industry Applications Society, 2018). However, this only occurs when the battery ESS is initially at 90% SoC and for the case of 25 ms time to full radiant intensity, therefore further investigation was conducted into the voltage deviation. This is presented in section 6.3.4. By design, the voltage ripple rms on the output bus is maintained below the 5% requirement defined by IEEE 1709. At 2 MW LDEW peak power the ripple is ± 20 V, or ± 1.3%. The design specification of the voltage ripple was to be within 1.4%, as detailed in section 4.12.1. The magnitude of the ripple is lower at lower load power. This can be explained by examination of (4.67). Hence ripple in $v_o$ is reduced compared to during the LDEW shot, for $398 < t < 398.75$ s in Fig. 6-4 (a).
6.3.3 Battery response to LDEW loading

The impact of the LDEW load and converter control system on the battery response is explored in this subsection. First, the battery voltage response is presented, followed by the battery current response.

Fig. 6-5 exhibits the voltage response of the battery ESS when subjected to a 2 MW, 1.75 MW and 1.5 MW LDEW load when the initial SoC is 90% (Fig. 6-5(a)) and 70% (Fig. 6-5(b)). For both cases \( T_r \) is 25 ms. Equivalent plots for when \( T_r \) is 50 ms and 100 ms are provided in Appendix U. Examination of Fig. 6-5(a) shows that prior to the LDEW firing at 17.5 s the ESS voltage is at steady state condition when supplying the 50 kW base load only. There is a voltage variation of \( \pm 3 \) V due to the effects of the switching of the devices of the DC/DC converter. For each of the LDEW load scenarios the initial voltage is approximately equivalent at 1,060 V. When the LDEW load is applied at 17.5 s, a voltage drop across the terminals of the ESS occurs, the magnitude of the voltage drop for each SoC initial setting in Fig. 6-5 is presented in Table 6-3. When the battery unloads following the firing of the LDEW, the battery voltage returns to a similar voltage level for each load scenario as the pre LDEW activation voltage.

There is not a distinct trend between \( T_r \) and the voltage drop of the ESS. However, the variable voltage characteristic of the battery ESS is evident in the magnitude of the voltage drop caused by the LDEW load scenarios, as shown in Table 6-3. This is because a larger current is drawn across the terminals of the ESS to meet the LDEW demand when the battery is at a lower SoC and thus lower voltage.
Comparing the overshoot in battery voltage drop for each LDEW load and initial SoC in Fig. 6-5 shows that under the 2 MW LDEW demand, the overshoot is larger than those for the 1.75 and 1.5 MW responses. This is due to the feedforward term in the control system inner loop, requiring the PI controller to respond more aggressively when responding to larger LDEW peak powers. Moreover, there is a larger overshoot between the 90% initial SoC condition compared with 70%, this is a consequence of the larger current drawn across a lower voltage requiring a more significant control action from the inner loop PI controller than at higher SoC.

Fig. 6-5. Battery voltage response to LDEW loading when the simulation commenced at (a) 90% and (b) 70% initial SoC when $T_r = 25$ ms

Table 6-3. Battery voltage drop corresponding to the results in Fig. 6-5

<table>
<thead>
<tr>
<th>Rise time</th>
<th>$T_r = 25$ ms</th>
<th>$T_r = 50$ ms</th>
<th>$T_r = 100$ ms</th>
</tr>
</thead>
<tbody>
<tr>
<td>Initial SoC</td>
<td>90</td>
<td>70</td>
<td>90</td>
</tr>
<tr>
<td>Peak LDEW power</td>
<td>Voltage drop (V)</td>
<td></td>
<td></td>
</tr>
<tr>
<td>2.00 MW</td>
<td>167</td>
<td>197</td>
<td>171</td>
</tr>
<tr>
<td>1.75 MW</td>
<td>142</td>
<td>157</td>
<td>140</td>
</tr>
<tr>
<td>1.50 MW</td>
<td>120</td>
<td>132</td>
<td>119</td>
</tr>
</tbody>
</table>

Fig. 6-6 presents the battery current response for the scenarios presented in Fig. 6-5, the battery voltage and current response for when $T_r$ is 50 ms and 100 ms are provided in Fig. U-1 and Fig. U-2 in Appendix U respectively. The current is increased when the initial SoC is lower due to the variable voltage characteristic of the ESS. Moreover, the lower voltage increases the magnitude of the ripple in the current drawn from the ESS as is evident in Fig. 6-6(b) when compared to Fig. 6-6(a). This can be explained mathematically by equation (4.71) in section 4.12.1.
6.3.4 Exploring the battery operating envelope for LDEW operation

This sub-section presents the results of the ability of the battery ESS to supply the energy required to meet the four minute LDEW operation requirement before the battery cut-off voltage is breached. This will be referred to as the battery operating envelope for the purpose of this novel investigation.

For this investigation, the initial SoC of the ESS was decreased incrementally in 10% steps between 90% and 50% SoC. For each SoC initial setting the battery ESS was simulated when subjected to each LDEW load and $T_r$ scenario. The test matrix is summarised in Table 6-4. The test matrix shows the simulations that were conducted. Simulations were conducted at 50% SoC for the 2 MW LDEW scenarios, however in each of these cases the cut-off voltage was breached prior to a single LDEW shot being completed, therefore the results were not credible and for this reason have not been included.

Table 6-4: Test matrix of successful simulations conducted in exploring the battery operating envelope under LDEW loading conditions

<table>
<thead>
<tr>
<th>$T_r$</th>
<th>$P_{LDEW}$</th>
<th>Initial SoC</th>
<th>90</th>
<th>80</th>
<th>70</th>
<th>60</th>
<th>50</th>
</tr>
</thead>
<tbody>
<tr>
<td>25 ms</td>
<td>2 MW</td>
<td>✓</td>
<td>✓</td>
<td>✓</td>
<td>✓</td>
<td>✓</td>
<td>x</td>
</tr>
<tr>
<td></td>
<td>1.75 MW</td>
<td>✓</td>
<td>✓</td>
<td>✓</td>
<td>✓</td>
<td>✓</td>
<td>✓</td>
</tr>
<tr>
<td></td>
<td>1.5 MW</td>
<td>✓</td>
<td>✓</td>
<td>✓</td>
<td>✓</td>
<td>✓</td>
<td>✓</td>
</tr>
<tr>
<td>50 ms</td>
<td>2 MW</td>
<td>✓</td>
<td>✓</td>
<td>✓</td>
<td>✓</td>
<td>X</td>
<td></td>
</tr>
<tr>
<td></td>
<td>1.75 MW</td>
<td>✓</td>
<td>✓</td>
<td>✓</td>
<td>✓</td>
<td>✓</td>
<td></td>
</tr>
<tr>
<td></td>
<td>1.5 MW</td>
<td>✓</td>
<td>✓</td>
<td>✓</td>
<td>✓</td>
<td>✓</td>
<td></td>
</tr>
<tr>
<td>100 ms</td>
<td>2 MW</td>
<td>✓</td>
<td>✓</td>
<td>✓</td>
<td>✓</td>
<td>X</td>
<td></td>
</tr>
<tr>
<td></td>
<td>1.75 MW</td>
<td>✓</td>
<td>✓</td>
<td>✓</td>
<td>✓</td>
<td>✓</td>
<td></td>
</tr>
<tr>
<td></td>
<td>1.5 MW</td>
<td>✓</td>
<td>✓</td>
<td>✓</td>
<td>✓</td>
<td>✓</td>
<td></td>
</tr>
</tbody>
</table>

Fig. 6-7 shows how the battery performs against the four-minute LDEW operation requirement with different values of initial SoC and $T_r$. The dashed horizontal line indicates 38 activations of the LDEW, which is equivalent to the four-minute requirement using the duty cycle and LDEW pulse duration examined in this research.
Fig. 6-7. Battery shot performance against $T_r$ and four minute operation requirement for (a) 2 MW (b) 1.75 MW and (c) 1.5 MW LDEW load.

When the LDEW peak power is 2 MW, the minimum initial SoC of the battery needs to be 83.0% and 81.5% for $T_r = 25$ ms and $T_r = 50$ ms respectively to satisfy the LDEW, four minute operation requirement (Fig. 6-7 (a)). Below these SoC levels the ESS is unable to meet the four minute operation requirement. For the 1.75 MW LDEW case in Fig. 6-7 (b), a minimum initial SoC of between 65.5%-66.7% is required to meet the four minute requirement. Under the 1.5 MW LDEW load, the battery ESS meets the duration requirement for all initial SoC settings between 90% and 50%. Further simulations conducted determined that 45% is the minimum SoC to meet the four minute requirement for the 1.5 MW LDEW load, this is not shown in Fig. 6-7. Summary of the initial SoC requirements is provided in Table 6-5.

<table>
<thead>
<tr>
<th>$T_r$</th>
<th>1.5 MW</th>
<th>1.75 MW</th>
<th>2 MW</th>
</tr>
</thead>
<tbody>
<tr>
<td>25 ms</td>
<td>46.7</td>
<td>66.7</td>
<td>83.0</td>
</tr>
<tr>
<td>50 ms</td>
<td>46.3</td>
<td>66.0</td>
<td>82.5</td>
</tr>
<tr>
<td>100 ms</td>
<td>45.0</td>
<td>65.5</td>
<td>81.5</td>
</tr>
</tbody>
</table>

The high di/dt of the LDEW load when $T_r$ is 25 ms, causes the cut-off voltage to be breached sooner than when $T_r$ is 50 ms and 100 ms, therefore the initial SoC is required to be higher for the 25 ms rise time to full radiant intensity. This is due to the control system dynamics discussed previously in section 6.3.3, causing a larger overshoot when the voltage drops, thus breaching the 800 V cut-off voltage.

Fig. 6-7(a) further demonstrates that useable energy in the battery ESS is drastically reduced for the 2 MW LDEW load case, as only one activation is available at 60% SoC. A maximum of 10 activations is available at 70% SoC, depending on the time to full radiant intensity. For the 1.75 MW load case, the battery ESS is able to deliver one LDEW activation at 50% SoC as shown in Fig. 6-7(b). The significance of Fig. 6-7 is
that it is a capability plot that allows an operator to identify the predicted ability of the battery ESS to power the LDEW load at BoL conditions.

6.3.5 QPS implications over the battery operating envelope

The QPS criterion of interest in this research is the output voltage deviation on the LDEW bus against the rated DC voltage of 1,500 V. As discussed in section 3.7.1, IEEE 1709 is the standard that pertains to DC power supply QPS for ship power systems between 1 kV and 35 kV and is therefore applicable to this investigation. As detailed in Table 3-10, IEEE 1709 recommends a voltage tolerance of within ±10% and voltage ripple rms of 5%. These are the two criteria, against which the QPS response of the system will be examined. The QPS results examined are applicable to the SoC operating envelope of the battery explored in section 6.3.4.

The output voltage deviation for each LDEW power level and time to full radiant intensity is presented in Fig. 6-8 when the initial battery SoC is 90%. For the time window shown between 60 and 80 s which comprises three LDEW shots, the magnitudes of all voltage deviations are demonstrated to be within the ±10% transient tolerance prescribed by IEEE 1709.

Fig. 6-8. Output voltage response to pulse load with (a)-(c) 25 ms, (d)-(f) 50 ms and (g)-(i) 100 ms time to full radiant intensity at 90% initial SoC. LDEW load of 2 MW in (a)(d)(g) 1.75 MW in (b)(e)(h) and 1.5 MW in (c)(f)(i)
There are three key deductions that can be made from Fig. 6-8. First, with increasing $T_r$ the voltage deviation reduces. Second, with decreasing LDEW power the voltage deviation decreases. Finally, the voltage ripple reduces as the magnitude of the LDEW load reduces. This can be explained by inspection of equation (4.67) in section 4.12.1, whereby the load resistance increases with reducing power therefore the voltage ripple reduces.

The output voltage deviation was recorded for each simulation conducted in Table 6-4. For each simulation the maximum positive and negative voltage deviations were recorded. The results are plotted in Fig. 6-9. As detailed in section Chapter 6 the negative deviation pertains to when the battery commences discharge in response to the LDEW load demand. The positive deviation pertains to the battery unloading and voltage recovery following an LDEW shot. The results for each $T_r$ in Fig. 6-9 are bounded with upper and lower limits. These limits capture the range of voltage deviation recorded for the test matrix in Table 6-4.

Fig. 6-9 shows that for each of the LDEW peak power and $T_r$ settings, $v_o$ is maintained within the transient voltage limits except for the when the LDEW peak power is 2 MW, and $T_r = 25$ ms. This case causes the system to exceed the 10% maximum tolerance which corresponds to the battery unloading during the LDEW load fall time. The larger deviation in this case can be attributed to the control system dynamics at the rising and falling edges of the LDEW pulse. The control system response is not fast enough to adequately track the step change in load current for the 2MW LDEW load that is fed forward to the inner current control loop, therefore the deviation magnitude of $v_o$ under the fast $T_r$ is of higher magnitude than for the 50 ms and 100 ms scenarios.
At peak LDEW power of 2 MW, when \( T_r \) is 50 ms and 100 ms, the \( v_o \) deviation is between -6% and +7% for the beginning of the LDEW activation and during unloading after the LDEW activation respectively. For the three LDEW peak powers tested, when \( T_r \) is 50 ms and 100 ms, the voltage deviation is less than ±7%.

Overall, the voltage QPS response presented for the operating envelope of the battery ESS could using Fig. 6-9, inform the system designer as to what degree a proposed system would function in compliance with the recognised guidance from IEEE 1709. The maximum deviation in \( v_o \) is 10.4%. However, it should be noted that this is at BoL condition of the battery ESS, where the battery ESS internal resistance has not had a chance to increase with cyclic and calendar aging. The input voltage to the DC/DC converter is at its highest under BoL conditions. Consequently, under degraded conditions where the internal resistance has increased, the dynamics of the system will be affected as a consequence of the change in voltage response. This warrants further investigation to understand how the performance of the battery ESS compares when degraded and how the change in internal resistance with aging affects the voltage QPS to consumers connected to the LDEW bus. This is explored as a novel investigation in section 6.4.4.

### 6.3.6 Exploring the cooling implications of LDEW loading on the battery ESS and converter

The investigation into the cooling implications consider the \( I^2R \) losses from the cells in the battery ESS and the heat generation due to the DC/DC converter as described by (6.1).

\[
Q_{batt} - \text{battery } I^2R \text{ losses } - \text{loss (DC filter + conduction + switching)} \rightarrow Q_{LDEW}
\]  

(6.1)

As detailed in section 3.4.7 and 4.4, the battery ESS is co-located with the LDEW, therefore for the purpose of this investigation the transmission line is considered short and the \( I^2R \) losses are assumed to be negligible. The results presented in this section are exclusive of LDEW losses, this is because the losses of interest in this investigation are focused on the battery ESS and the DC/DC converter interface to the LDEW load. Modelling tool 3 was used to determine the battery ESS and converter losses for each LDEW condition. To capture the ESS \( I^2R \) losses, the efficiency map polynomial function used in modelling tool 1 was integrated with the dynamic battery model in modelling tool 3. The efficiency across the converter was calculated using the ratio of the power in to power out.

The energy lost in the system due to waste heat was determined for each LDEW peak pulse power when \( T_r = 25 \) ms and the initial SoC is 90%. The run time was 250 s for each simulation. This permits the initialisation of the simulation in the first 5 s, and the proceeding LDEW firing sequence allows results to be captured that are indicative of the performance of the system throughout four minute operational scenarios. This is significant as the impact of quantifying the heat generated is useful information to those responsible for the design of the battery ESS and cooling system.

Fig. 6-10(a) shows the energy supplied by the ESS over 250 s of LDEW operation compared to the energy delivered to a 2 MW LDEW load and the 50 kW base load. The delta in energy supplied to energy delivered...
at 250 s is 24 MJ. The combined LDEW and base load are presented in Fig. 6-10(b). The efficiency of the ESS over the scenario is shown in Fig. 6-10(c), which shows that the efficiency under low load is ~99% and is ~96% when firing an LDEW shot. Similar plots for the 1.75 MW and 1.5 MW LDEW load scenarios are provided in Appendix V.

![Energy supplied and delivered](image1)

![Load power](image2)

![ESS efficiency](image3)

Fig. 6-10. (a) Energy supplied and delivered (b) load power and (c) ESS efficiency during four minute operation of LDEW at peak 2 MW pulse power when ESS initial SoC = 90% and \( T_r = 25 \) ms.

Table 6-6 summarises the system heat generation implications when the battery is providing power from an initial SoC of 90% with \( T_r = 25 \) ms. The energy delivered was 5%, 9.6% and 11% less than the energy supplied by the ESS for the 1.5 MW, 1.75 MW and 2 MW LDEW load scenarios respectively. The converter maintains an efficiency at 98% for each LDEW load scenario, whilst the ESS mean efficiency is ~98%. The efficiency of the DC/DC converter appears high, however when the peak load is being discharged, the duty cycle of the boost converter is less than 0.5 (see (3.17) in section 3.6.1), this is the region of highest efficiency and comparable to results presented in Kazimierczuk (2016) for the boost converter topology used in this research.

The mean heat loss is 2.2%, 2.3% and 2.5% of the LDEW peak powers of 1.5 MW, 1.75 MW and 2 MW respectively. While this is not significant relative to the LDEW power, the measured peak loss for each case is 2.84, 3 and 3.16 times more than the mean heat loss for the respective LDEW peak powers.

A worse case scenario from a heat generation perspective during LDEW operation for the candidate ship could arise if the LDEWs on both the forward and aft switchboards simultaneously demand 2 MW from their respective dedicated battery ESS. In this scenario the combined peak heat generated would be 310
kW. The transient nature and magnitude of the cooling requirement determined here will have implications for the chilled water system design, particularly the capacity of the plant to manage pulsed thermal loading.

Table 6-6. Results at the end of four minutes using 90% SoC case ($T_r$=25 ms)

<table>
<thead>
<tr>
<th>Performance parameter</th>
<th>1.5 MW</th>
<th>1.75 MW</th>
<th>2 MW</th>
</tr>
</thead>
<tbody>
<tr>
<td>Total energy supplied (MJ)</td>
<td>171</td>
<td>205</td>
<td>242</td>
</tr>
<tr>
<td>Total energy delivered to load (MJ)</td>
<td>162</td>
<td>187</td>
<td>218</td>
</tr>
<tr>
<td>ESS mean efficiency (%)</td>
<td>98.0</td>
<td>97.9</td>
<td>97.9</td>
</tr>
<tr>
<td>ESS minimum efficiency (%)</td>
<td>96.3</td>
<td>96.2</td>
<td>96.2</td>
</tr>
<tr>
<td>ESS waste heat over the scenario (MJ)</td>
<td>5.9</td>
<td>7.2</td>
<td>8.2</td>
</tr>
<tr>
<td>ESS mean heat loss (kW)</td>
<td>24</td>
<td>29</td>
<td>33</td>
</tr>
<tr>
<td>ESS peak heat loss (kW)</td>
<td>61</td>
<td>73</td>
<td>83</td>
</tr>
<tr>
<td>Mean converter efficiency (%)</td>
<td>98.3</td>
<td>98.4</td>
<td>98.3</td>
</tr>
<tr>
<td>Total mean heat loss (kW)</td>
<td>33</td>
<td>40</td>
<td>49</td>
</tr>
<tr>
<td>Total peak heat loss (kW)</td>
<td>94</td>
<td>120</td>
<td>155</td>
</tr>
</tbody>
</table>

6.3.7 Exploring wider power system implications of the ESS for LDEW power supply: recharging and system efficiency

This subsection considers three of the effects that powering the LDEW has on the candidate ship power system. First, the recharge time of the ESS. Second, the system efficiency when charging the ESS, and therefore the power required to be drawn from the switchboard. Third, the overall system efficiency from charging the ESS through discharging from the ESS to the LDEW.

Following an LDEW engagement, if there is an operational need from the ship to have the LDEW available for a subsequent LDEW engagement, then the battery ESS needs to be recharged at the maximum permissible charge rate to ensure timely operational availability of the battery to supply power to the LDEW. The maximum charging current allowed by the cells in the battery system is 3C (1,536 A for the entire ESS). The recharge power profile will follow the variable voltage characteristic of the cells in the system between 20% and 90% SoC, this is because the battery ESS is charged at constant current in this SoC range. The duration of recharge required is dependent on the potential operating time of the LDEW for the subsequent engagement. For the purpose of this analysis, the required operating time of the subsequent engagement is assumed equal to the four-minute requirement specified by Markle (2018). It is also assumed that the battery ESS design is capable of maintaining the battery cells within their operating temperature range for two engagements and the recharge phase in between. The minimum time required to recharge prior to the next engagement depends on the initial SoC at the beginning of the first engagement. It is this time period that is a main topic of interest in this analysis. This is presented in Fig. 6-11 (note this does not include the time for the generators to ramp up to maximum recharge power). It should be noted
that fast charging at high current levels is known to have a negative effect on electrochemical performance, with the potential to cause excessive heat generation and cell degradation via lithium plating or SEI layer breakdown (Birkl et al., 2017; Liu et al., 2019).

Fig. 6-11 shows the recharge time required for the ships’ generators to deliver enough energy to recharge a battery ESS to the minimum level of SoC that would allow a second engagement to take place for each LDEW peak power. For example, Fig. 6-11 shows that for the 2 MW LDEW load case the ESS is limited to a minimum initial SoC of 83% before an engagement can take place for four minutes. If a subsequent four-minute engagement was required for operational reasons, the recharge time would be 201 s until there is sufficient energy stored in the battery system. This shows the operational advantage of maintaining the ESS at the highest SoC possible when in State 1 at action stations. Alternatively, a cell with higher recharge rate capability would be advantageous to minimise the recharge time. A set of the full recharge results is provided in Appendix W.

![Fig. 6-11. Recharge time to minimum SoC at maximum charge rate of 3C that permits a second four minute engagement](image)

The recharge power requirement is a maximum of 1.5 MW at the ESS terminals following an LDEW engagement, as with the discharge characteristic of the ESS, the voltage during charge is variable and therefore so is the recharge power. The minimum recharge power is 1.45 MW and occurs following a 1.5 MW LDEW engagement where the ESS is at 35% SoC and ESS voltage is 940 V. Fig. 6-12 shows the system efficiency during charging from the main switchboard to the ESS, including the losses of the transformer, LCL filter, VSC and ESS I/R losses at peak charge rate. Using Fig. 6-12, the recharge power drawn from the switchboard is therefore between 1.6 and 1.65 MW depending on SoC at the discrete moment of recharge.

The load per switchboard during State 1, assuming mechanical propulsion, equal load sharing between switchboards and all four generators on-line, is 1.75 MW during winter conditions as the highest demand case. Consequently, during the recharge phase following an LDEW engagement the load demand on one switchboard including ship services and recharging power is 3.4 MW. In tropical and temperate conditions the combined loads are 3.125 MW and 3 MW respectively. Therefore, in winter and tropical conditions,
the allowable loading margin for the N-1 failure condition for power reserve is exceeded. This may necessitate load shedding logic of non-essential loads if the ESS is required for a second successive LDEW engagement. The loading margin is not exceeded in temperate conditions.

Fig. 6-12. Efficiency from the main switchboard to the ESS during charging at peak C-rate (includes ESS I^2R losses)

From a system efficiency perspective, charging at higher current rates provides higher overall charging efficiency from the switchboard to the ESS. Consider Fig. 6-13, if the battery is trickle charged at 0.5C, which prevents degradation mechanisms from aging the battery, the charging energy efficiency at low rates is approximately 86% compared to 90-91% as shown in Fig. 6-12.

Fig. 6-13. Charging efficiency map from the switchboard to the ESS (includes ESS I^2R losses)

The Sankey diagram in Fig. 6-14, which shows the breakdown of the system efficiency from charging the ESS at peak charge rate to discharging to the LDEW, the overall efficiency is 85%. Conversely, when charging at 0.5C the overall efficiency is 77%. The LDEW losses are not included in the Sankey diagram. These reportedly range from 20-30% (Gattozzi et al., 2015; O’Rourke, 2018; Titterton, 2015).
6.3.8 Beginning of life case study results summary

The results presented in section 6.3.1 demonstrate how the system under investigation responds to the 2MW LDEW load profile when the ESS starts the simulation at 90% initial SoC, and the LDEW peak power is 2 MW with 25 ms time to full radiant intensity. This condition reflected the worst case load scenario for the best case condition of the ESS. Three key aspects were highlighted. First, the battery ESS was able to facilitate a four minute LDEW engagement requirement. Second, as predicted in section 3.5.2, the rise time and magnitude of the LDEW load is sufficient to prevent the battery to limitlessly facilitate the LDEW load. This limit was found to be 68% SoC, because of the ohmic voltage drop of the ESS. This equated to an engagement time of 6.8 minutes. Third, the output voltage deviation was exceeded under the above conditions. This occurred at the unloading of the battery ESS due to the inertial energy in the system causing overvoltage on the output LDEW bus following an LDEW shot.

The results in Fig. 6-7 showed that the shot constraints under LDEW loading conditions are a factor of the design specifications and operational state of the ESS depending on the initial SoC, peak power of the LDEW load and time to full radiant intensity of the laser aperture. To meet the four minute LDEW operation requirement, the initial SoC for a BoL system must not be less than the limits stated in Table 6-5. This is due to the variable voltage characteristic of the battery ESS and the voltage drop at the battery ESS terminals under LDEW loading.

To comply with IEEE 1709, the constraint on $T_r$ of the 2 MW LDEW is 50 ms. The peak output voltage deviation recorded for the 2 MW LDEW load case was +10.4% when $T_r$ is 25 ms. This occurs when the ESS is unloaded following an LDEW shot as shown in Fig. 6-4.

The results presented in Fig. 6-11 provide a reference to the operator of the recharge time required between four minute LDEW engagements, this is dependent upon the initial SoC of the ESS at the first engagement. The maximum recharge times between two, four minute engagements when recharging at the maximum charge rate of the ESS are 3 mins 31 s, 2 mins 50 s and 2 mins 23 s for the 2 MW, 1.75 MW and 1.5 MW LDEW loads respectively. Regardless of initial SoC, the battery ESS is unable to provide two successive four minute 2 MW LDEW load engagements without requiring recharge. The minimum time in this case is 1 min 36 s when the initial SoC is 90%. The minimum ESS initial SoC to provide two successive four
minute engagements without a pause to recharge is 78% and 55% for the 1.75 MW and 1.5 MW LDEW peak loads respectively.

The results in Table 6-5 identified the heat generated by the system components under investigation at BoL conditions when discharging for a four minute engagement scenario. For recharging conditions, it was shown in Fig. 6-13 that it is beneficial from an efficiency perspective to recharge at higher current rates. At lower charge rates the efficiency when charging the ESS from the switchboard to discharging to the LDEW load can be reduced from 85% down to 77%.

### 6.4 Degraded battery system investigation

The first study of this chapter presented an assessment of the ESS at BoL conditions to meet LDEW demands. The repetitive, high current discharge rate conditions that the ESS is subjected to by the LDEW could promote cell degradation due to cyclic loading, whereby the cell storage capacity fades and internal resistance increases as discussed in Huhman et al. (2016). Consequently, aging affects will influence whether the LDEW can operate for four minutes when the ESS is within the SoC ranges established in this research. Further, the auxiliary cooling load will increase due to the change in cell internal resistance. It is not known how cell degradation would impact the output QPS for the system under investigation in this chapter. This section will present the results of novel research conducted into the impact of LDEW loading on the performance of the battery ESS and converter dynamics when the battery ESS is degraded to a significant extent from its original BoL state.

#### 6.4.1 Investigation description

The aim of this part of the investigation is to assess how the performance envelope of the battery system changes from the study presented in section 6.3, following illustrative degradation. The investigation into how the performance envelope of the battery system changes following illustrative degradation was conducted by simulating the system and applying degradation factors to the cell capacity and the cell internal resistance. The degradation factors used in this investigation were applied based on resistance increase and capacity fade data from a degradation study presented by de Hoog et al. (2017) and de Hoog et al. (2018) for a 4.2 V NMC based Li-ion pouch cell rated at 20 Ah nominal capacity under conditions maintained at 25°C. The referenced work was selected because the cells used in this investigation and in their research both utilise NMC chemistry and the electrical performance parameters are comparable. Table 1 in the paper presented by de Hoog et al. (2017) details the electrical characteristics of the cell tested by the authors. The key parameters are summarised in Table 6-7 and compared against the cell used in this research. The explanation for the difference in capacity and maximum discharge rate is the lower internal resistance of the cell in this research. Lower internal resistance permits higher discharge rates as high power is inversely proportional to internal resistance.
Table 6-7. Difference in cells tested in de Hoog et al. (2017) and this research

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Cell in de Hoog et al. (2017)</th>
<th>Cell in this research (details provided by Corvus Energy)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Nominal capacity (Ah)</td>
<td>20</td>
<td>64</td>
</tr>
<tr>
<td>Peak voltage (V)</td>
<td>4.2</td>
<td>4.2</td>
</tr>
<tr>
<td>Cut-off voltage (V)</td>
<td>3</td>
<td>3</td>
</tr>
<tr>
<td>Maximum discharge rate (C-rate)</td>
<td>5</td>
<td>6</td>
</tr>
<tr>
<td>Internal resistance (mΩ)</td>
<td>3</td>
<td>1</td>
</tr>
</tbody>
</table>

The referenced authors analysis considered the degradation performance of a total of 146 of the same type of NMC cell from the same manufacturer, of the 146 cells tested, 23 cells were tested at 25°C. The remainder were tested at -10°C, 0°C, 35°C, 45°C and 50°C. The degradation data for the cells tested in the 25°C experiment was selected because this is the temperature closest to that recorded in the experimental testing that formed the basis of the dynamic cell model used in this research. Temperature data for the latter is shown in Table 4-18 of section 4.13.1. To represent the worst case degradation condition and to align with the maximum DoD permitted in this research, the cells cycled to 80% DoD (i.e. 20% SoC) were selected from de Hoog et al. (2017), of which there were 8 cells studied. The reference data for storage capacity fade can be found in Fig. 4. (a) of de Hoog et al. (2017). Fig. 7(a) of de Hoog et al. (2018) presents the internal resistance increase. The subsequent parameters used as the basis of degradation testing in this research are shown in Table 6-8.

Table 6-8. Degraded condition test matrix based on de Hoog et al. (2017) and de Hoog et al. (2018)

<table>
<thead>
<tr>
<th>Condition</th>
<th>Internal Resistance Increase (%)</th>
<th>Capacity Decrease (%)</th>
<th>Cycles to 80% DOD (#)</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>1</td>
<td>2.5</td>
<td>500</td>
</tr>
<tr>
<td>2</td>
<td>1.5</td>
<td>4.5</td>
<td>1,000</td>
</tr>
<tr>
<td>3</td>
<td>7</td>
<td>6</td>
<td>1,500</td>
</tr>
<tr>
<td>4</td>
<td>16</td>
<td>8</td>
<td>2,000</td>
</tr>
<tr>
<td>5</td>
<td>19</td>
<td>10</td>
<td>2,500</td>
</tr>
</tbody>
</table>

Fig. 6-15 presents the internal resistance impact of condition 5 on the power fade of the cell under investigation in this research compared to the BoL condition. Importantly the cell maximum power capability remains above 2 MW for the 90-20% SoC range limit that is applied in this research (see section 4.2.3).
Capacity degradation was captured in the cell dynamic model used in modelling tool 3 by introducing a capacity degradation factor, $k_Q$ to (4.1). Equation (4.1) is used to estimate state of charge of the cell in the model, where $Q$ is the battery capacity in Ah. Therefore (4.1) becomes:

$$s_Q = s_{Q0} - \frac{1}{Qk_Q} \int i_{bat}$$  \hspace{1cm} (6.2)

Where $k_Q$ is a multiplier that reflects the capacity decrease detailed in Table 6-8. To apply the internal resistance degradation characteristics in Table 6-8, the multiplying factor $k_R0$ was applied to the internal resistance parameter map presented in Fig. 4-13.

In this investigation, modelling tool 3 was simulated with 25 ms rise time for each of the degraded conditions in Table 6-8 to determine how the battery operating envelope for the LDEW diminishes with cyclic aging. The 25 ms rise time case was used to investigate the worst case condition. Three explorations comprise the degraded condition investigation. First, that of the battery SoC operating envelope. Second, the degradation impact on output voltage QPS and finally, the thermal implications of a degraded battery ESS during discharge.

The investigation was conducted by incrementing the initial SoC, running the simulation and then evaluating whether the four minute requirement was achieved before either the cut-off voltage, current limit, or SoC termination criteria are met. It should be noted that the cell degradation modes influence the characteristics of the cell OCV, depending on whether a loss of lithium inventory, or loss of active material of the anode or cathode has occurred (Birkl et al., 2017). These degradation modes depend on the initial degradation cause and the degradation mechanism. For the purpose of this investigation it has been assumed that the nominal OCV between 90% and 20% SoC has not been affected by the degradation modes. Information on how degradation modes affect the OCV can be found in Birkl et al. (2017), particularly Figs. 3 and 5 therein.
6.4.2 Exploring the degraded battery operating envelope

In a similar manner to the investigation in section 6.3.4, this sub-section presents the results of the ability of the battery ESS to supply the energy required to meet the four minute LDEW operation requirement before the battery cut-off voltage is breached under degraded conditions. This is defined as the degraded battery operating envelope.

The operating envelope in section 6.3.4 was used as the starting point from which to investigate the degraded battery operating envelope. The novel investigation followed a sequential approach:

1) Apply degradation factors of condition 1 and simulate the battery ESS at 2.5% SoC increments near the minimum initial SoC found in the BoL condition. Identify lowest SoC value that can deliver four minute activation of LDEW without exceeding pre-set limits.

2) Repeat for condition 2 with respect to the minimum initial SoC found in condition 1.

3) Repeat for all remaining degradation conditions.

For all simulations 2.5% increments were used, the exception being for the 2 MW LDEW case in condition 5, where 1% increments were used. This is due to the proximity of the results found in condition 4 to the maximum allowable SoC of 90% in this research. Smaller SoC increments were considered inappropriate for the remaining simulations due to the imposition on computational time resources.

Fig. 6-16 exhibits how the degradation factors applied in this investigation could affect the minimum initial SoC range that is capable of achieving the four minute LDEW operation requirement. Fig. 6-16 is significant as degraded performance of battery ESS under LDEW loading has been previously unknown. The left-hand y-axis shows the capacity of each condition relative to the BoL. The operating envelopes for each LDEW power and degradation condition are shown by the bar plots and relate to the capacity on the left-hand y-axis. The internal resistance rise is shown on the right-hand y-axis. The full table of results for the degraded case study can be found in Appendix X.
Fig. 6-16. Battery system operating envelope with degradation condition and LDEW power to meet LDEW operational requirement of four minutes. The 2MW, 1.75 MW and 1.5 MW results pertain to the capacity on the left-y axis.

The degradation conditions 1 and 2 where the internal resistance increases are 1% and 1.5% respectively have minimal impact on the minimum initial SoC to achieve the four minute requirement relative to the BoL condition. However, at condition 3, where the internal resistance has increased by 7%, the initial SoC window is visibly reduced for the 2 MW LDEW case. The most significant reductions are seen in conditions 4 and 5. This is a consequence of the voltage drop increase due to the increased internal resistance through degradation compared to the BoL condition. Voltage cut-off of the cells in the ESS is attained more quickly due to the higher internal resistance of the cell. This therefore reduces the useful cell capacity under the LDEW load. For all conditions the results in Fig. 6-16 the indication is that theoretically the battery ESS may be able to satisfy the LDEW four minute requirement, but over a reduced range of conditions. A summary of the initial SoC requirements that pertain to the results in Fig. 6-16 is presented in Table 6-9.

Table 6-9. Battery minimum initial SoC operating envelope under BoL and degraded conditions.

<table>
<thead>
<tr>
<th>Condition</th>
<th>Capacity at condition (kWh)</th>
<th>Minimum initial SoC (%) for each LDEW peak power relative to degraded capacity</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td>1.5 MW</td>
</tr>
<tr>
<td>BoL</td>
<td>500.0</td>
<td>46.7</td>
</tr>
<tr>
<td>1</td>
<td>487.5</td>
<td>45.0</td>
</tr>
<tr>
<td>2</td>
<td>477.5</td>
<td>45.0</td>
</tr>
<tr>
<td>3</td>
<td>470.0</td>
<td>47.5</td>
</tr>
<tr>
<td>4</td>
<td>460.0</td>
<td>55.0</td>
</tr>
<tr>
<td>5</td>
<td>450.0</td>
<td>57.5</td>
</tr>
</tbody>
</table>
### 6.4.3 Impact of degradation condition on system QPS performance

Two comparisons were made when considering the BoL against the degraded condition QPS performance. The first considered the direct comparison of a four-minute simulation conducted at 90% initial SoC for each condition. Making the comparison for the 90% initial SoC condition is justified because the ESS cannot be less than 89% to meet the four minute LDEW operation requirement. The maximum deviation results are presented in Table 6-10. The increase in voltage deviation, although marginal, follows the trend of internal resistance degradation, for the 1.5 MW, 1.75 MW and 2 MW LDEW cases. Importantly this comparison shows how the QPS may be impacted through the life of the battery ESS. For the 1.5 MW and 1.75 MW LDEWs the voltage fluctuations are not large enough to surpass the 10% transient tolerance limits recommended by IEEE 1709 for the first case examined when the battery is at 90% SoC and \( T_r = 25 \) ms. For the LDEW rated at 2 MW the 10% tolerance threshold is breached when the ESS is in condition 5.

<table>
<thead>
<tr>
<th>Condition</th>
<th>Maximum QPS deviation from nominal voltage at the output DC bus</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>1.5 MW</td>
</tr>
<tr>
<td>BoL</td>
<td>-5.50%</td>
</tr>
<tr>
<td>1</td>
<td>-5.53%</td>
</tr>
<tr>
<td>2</td>
<td>-5.55%</td>
</tr>
<tr>
<td>3</td>
<td>-5.60%</td>
</tr>
<tr>
<td>4</td>
<td>-5.70%</td>
</tr>
<tr>
<td>5</td>
<td>-5.80%</td>
</tr>
</tbody>
</table>

The second comparison includes the results at minimum initial SOC in Section 6.3 and 6.4, therefore representing the worst case scenario. The results of this are presented in Table 6-11. For the 1.5 MW, 1.75 and 2 MW LDEWs. The maximum recorded deviation increased by 0.25%, 0.15% and 0.35% respectively from BoL to condition 5.

<table>
<thead>
<tr>
<th>Condition</th>
<th>Maximum QPS deviation from nominal voltage at the output DC bus</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>1.5 MW</td>
</tr>
<tr>
<td>BoL</td>
<td>-6.90%</td>
</tr>
<tr>
<td>1</td>
<td>-6.93%</td>
</tr>
<tr>
<td>2</td>
<td>-6.94%</td>
</tr>
<tr>
<td>3</td>
<td>-6.98%</td>
</tr>
<tr>
<td>4</td>
<td>-7.15%</td>
</tr>
<tr>
<td>5</td>
<td>-7.07%</td>
</tr>
</tbody>
</table>
The results of this investigation demonstrate that the control system dynamics are not significantly affected by the degradation modes and that the maximum voltage deviation recorded under the 2 MW LDEW loading is still close to the recommended deviation limit as recommended by IEEE 1709.

6.4.4 Degraded performance consequences on the battery efficiency and system cooling

The increase in internal resistance of the cell due to degradation will increase the $I^2R$ losses from the cells in the battery ESS. As discussed in section 3.5.1 and as shown by (3.6), the increased voltage drop due to the increase of the internal resistance will draw higher current from the cells, exacerbating the losses and reducing the cell efficiency compared to the BoL condition.

For each degraded condition in Table 6-8 the efficiency maps of the cell were generated using the process described in section 4.6.4 with reduced cell capacity and increased internal resistance. Comparison of how the efficiency map changes from BoL to condition 5, where the internal resistance of the cell has increased by 19% is shown in Fig. 6-17 (a), the efficiency delta is shown in Fig. 6-17(b). Note that the SoC of the cell in condition 5 in Fig. 6-17(a) has been normalised with respect to the degraded cell capacity. At low discharge rates the cell efficiency drop is approximately 1%. The largest efficiency delta occurs at the lowest SoC and highest discharge rate, being 5% lower than BoL.

![Fig. 6-17. (a) BoL and condition 5 cell efficiency maps (b) delta between BoL and condition 5 efficiency maps](image)

The impact of internal resistance increase on the efficiency of the battery ESS under LDEW operation was compared for the case where the ESS commences an LDEW engagement at an initial SoC of 90% for a period of four minutes. The impact of the internal resistance on the minimum efficiency during an LDEW engagement and therefore peak losses is shown in Fig. 6-18(a) and Fig. 6-18(b) respectively. A full table of results for the degraded condition is provided in Appendix X. As the ESS is at high SoC during the engagement, the change in minimum efficiency between BoL and condition 5 is relatively small at 1%, which can be explained by examining Fig. 6-17(b). Due to the scale of the load however, the consequence of the 1% decrease in efficiency results in a 22 kW increase in heat generation for the 2 MW LDEW case. For each LDEW peak power the peak thermal generation is 27% higher at condition 5 than at BoL.

The efficiency reduction characteristic exhibited in Fig. 6-18(a) aligns with the rise in internal resistance, due to degradation. Fig. 6-18(c) presents the $I^2R$ loss over the entire four minute engagement and how this characteristic increases with degradation condition. In condition 5 the increase in $I^2R$ losses are 24%, 27% and 25 % for the 1.5, 1.75 and 2 MW LDEW loading conditions.
Degraded battery system investigation results summary

The results of this section of the LDEW power supply investigation have demonstrated how the system performance changes when the battery based ESS has degraded. This section has demonstrated how the degraded conditions detailed in Table 6-8 impact the battery operating envelope when meeting the demands of a four-minute LDEW engagement. The diminishing SoC envelope where the battery ESS is able to provide the power and energy requirement for a four minute engagement without breaching the cut-off voltage was shown in Fig. 6-16. It was shown that whilst the ESS was still able to meet the demands of the LDEW for all the peak powers examined, at the worst case degradation condition the battery ESS is limited to an initial SoC window of 1% SoC, between 89 and 90% SoC for a 2 MW LDEW load. It has been demonstrated how battery ESS degradation increases the output voltage fluctuation for the system under investigation. In maintaining the fluctuation below the 10% tolerance, the time to full radiant intensity must be above 25 ms. Operating the LDEW at 50 ms rise time was shown to maintain the voltage fluctuation to within 8.6% when the battery ESS is at its worst degradation condition, which satisfies IEEE 1709. Under discharging conditions it was shown that degradation of the ESS results in an increase of the peak heat production by 27%.

Fig. 6-18. (a) Minimum recorded ESS efficiency (b) peak ESS heat loss and (c) total FR loss during four minute LDEW operation when the initial battery SoC is 90% and $T_r=25$ ms

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6.5 Implications and consequences

The impact of operating an LDEW with the candidate ESS via a DC/DC converter has been investigated in this chapter. The investigation was conducted in two parts, first when the battery is at BoL conditions and second, under degraded conditions. This investigation explored five research problems related to meeting the demands of anticipated LDEW load conditions as specified in Table 3-9 and section 6.2.1. The themes of the respective research problems being the dynamic response of the system under LDEW loading conditions, the ability of the ESS to meet the energy requirement of the LDEW, the heat loss of the battery ESS and DC/DC converter in meeting LDEW demands, and the subsequent recharge impact on the candidate power system. This section considers the implications and consequences of these themes when using the candidate battery ESS in the candidate warship power system described in section 3.2.

As discussed in section 3.7.1, compliance of the output DC bus from the DC/DC converter with the transient tolerance limits of IEEE 1709 provides system integrity, as adherence ensures correct operation of equipment when the system is operating within these tolerance limits. It is apparent from the results presented in Fig. 6-9 and Table 6-11 that the time to full radiant intensity of the LDEW coupled with the peak output power of the LDEW causes these limits to be exceeded under certain operating conditions. Therefore, the following implications can be drawn from a QPS perspective when using the candidate battery ESS and single stage DC/DC boost converter in the candidate electric power system comprising an LDEW load:

1) Fig. 6-9 shows that for the BoL operating envelope of the battery ESS, operating a 2 MW LDEW with time to full radiant intensity of 25 ms would result in the voltage exceeding the IEEE 1709 voltage deviation limit. These results show an over-voltage response.

2) Table 6-11 shows that operating a 2 MW LDEW with time to full radiant intensity of 25 ms when the battery has degraded would result in the voltage exceeding the IEEE 1709 voltage deviation limit. The results in Table 6-11 show an over-voltage response.

3) As shown in Fig. 6-9 for the BoL operating envelope of the battery ESS, operating a 2 MW LDEW with time to full radiant intensity of 50 ms satisfies voltage deviation requirements defined in IEEE 1709.

4) Fig. 6-9 and Table 6-11 also shows that the battery ESS would be capable of being the power supply to a 1.75 MW load with a time to full radiant intensity of 25 ms at BoL and degraded up to 10% capacity decrease and 19% internal resistance increase.

Therefore, the implication of integrating a 2 MW LDEW with 25 ms time to full radiant intensity and using the candidate ESS to power the LDEW load via a single stage DC/DC converter from a QPS perspective results in over voltage. For the other LDEW load cases examined the QPS criteria are satisfied, both at BoL and during degraded conditions of up to 10% capacity decrease and 19% internal resistance increase of the ESS.

As first discussed in section 3.5.2, from a dynamic performance perspective, the high current and fast rise time characteristic of the LDEW load is an extreme loading condition that has the potential to limit the SoC
operating range of the battery ESS. Increasing the number of battery strings could extend the operating envelope under LDEW loading. However, as discussed in section 4.6, four parallel strings are the limit of the battery ESS due to the fault current contribution of the strings. For more than four strings, DC breakers are required to limit the fault current contribution as the independent fuses on the strings are no longer capable of providing the required fault protection. Conventional DC breakers are large, costly and are relatively slow acting (Meggs and Pollard, 2016). Solid state circuit breakers aim to reduce the fault detection and isolation time, however challenges still exist with power density (Cairoli et al., 2019). The SoC range is limited due to the voltage drop induced by the load causing the ESS protection logic to trip the ESS on under voltage due to breach of the ESS voltage cut-off. As discussed in section 2.1.1, the requirement for ESS to operate an LDEW is to provide sufficient energy to meet a four-minute engagement (Markle, 2018). The following significant implication can be drawn from the results of this chapter:

1) Table 6-9 specifies the minimum allowable initial SoC of the battery ESS for the 1.5 MW, 1.75 MW and 2 MW LDEW load conditions that permit a four minute LDEW operation when the battery ESS is at BoL and under degraded conditions. This is novel and has been previously unknown for the candidate battery ESS. This is a function of the internal resistance of the cells, and the evolution of the internal resistance characteristic as the cell changes with cyclic and calendar aging.

As discussed in section 3.7 there is a need to determine the thermal loss characteristics of the battery ESS and DC/DC converter under LDEW loading conditions. Powering the LDEW from the candidate ESS via a DC/DC converter has the following implication:

1) The results in Table 6-6 detail the amount of heat that requires removal to ensure the cells and components of the DC/DC converter operate in ambient conditions when the ESS is at BoL. This is significant because the results inform the cooling system designer of the minimum heat the battery ESS will generate when powering the LDEW.

2) The results in Table 6-6 and Fig. 6-18 details the peak heat load that would need to be managed by a cooling system powered by the candidate warship power system for the battery ESS and DC/DC converter when the battery ESS is degraded. The peak ESS heat generated under LDEW loading increases by 27% when the internal resistance increases by 19% compared to BoL conditions. This is a significant finding as this provides an estimate to the cooling system designer of the heat that could be generated by a degraded battery ESS when powering the LDEW.

The final aim of this investigation was to quantify the impact of the LDEW on the candidate warship power system during recharging. This is significant as the impact relates to the recovery time to operational availability of the LDEW. The following implications can be drawn from the results presented in this investigation:

1) Fig. 6-11 shows that when the ESS is above 55% and 77% initial SoC the ESS is able to power two successive four minute LDEW engagements for the 1.5 MW LDEW and 1.75 MW LDEW respectively.

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2) Fig. 6-11 shows the shortest recharge times at maximum recharge rate of the ESS to permit a second LDEW engagement, this depends on the initial SoC of the previous engagement.

3) Fig. 6-13 shows that recharging the ESS following an LDEW engagement at maximum recharge rate offers the highest efficiency compared to lower charging rates.

A summary of the above discussion on the key implications of the results generated in this chapter is offered in Table 6-12. The consequences that relate to the implications are also presented. Recommendations relating to the consequences of using the candidate battery ESS to power LDEWs in warship power systems is provided in the following section and concludes this chapter.

Table 6-12. Summary of the key implications and consequences resulting from using the candidate battery ESS to power LDEW in warship power systems

<table>
<thead>
<tr>
<th>Implication</th>
<th>Consequence</th>
</tr>
</thead>
<tbody>
<tr>
<td>Using the candidate ESS to power a 2 MW LDEW with time to full radiant intensity of 25 ms would result in the voltage exceeding the IEEE 1709 voltage deviation limit resulting in an over-voltage situation.</td>
<td>It is likely that loads connected to the DC power source will be designed to operate within the constraints of IEEE 1709. Departure from the recommended voltage limits could trigger protection relays of equipment connected to the LDEW DC bus.</td>
</tr>
<tr>
<td>The ESS SoC cannot be less than the values specified in Table 6-9 at the commencement of an LDEW engagement lasting four minutes.</td>
<td>Transferring the energy required for a four minute LDEW engagement when the SoC is outside these limits would result in breaching the cut-off voltage. Operating at cell voltage below cut-off can cause corrosion of current collectors, loss of electric contact and lithium plating in the cells of the battery ESS resulting in accelerated capacity and power fade (Birkl et al., 2017).</td>
</tr>
<tr>
<td>The cooling system of the ESS needs to be sufficient to facilitate the heat loss detailed in Table 6-6, with a minimum 27% margin to account for battery ESS degradation.</td>
<td>Insufficient heat removal by the cooling system could cause the cells in the battery ESS to operate outside of their designed temperature range. Operating the cells at elevated temperatures can cause electrolyte decomposition, binder decomposition and SEI layer growth in the cells resulting in accelerated capacity and power fade (Birkl et al., 2017).</td>
</tr>
<tr>
<td>The minimum recharge time of the battery ESS at BoL to provide two successive LDEW engagements is defined by Fig. 6-11.</td>
<td>Recharging the ESS at a faster rate than the designed charge acceptance rate of the cell can promote SEI layer growth, structural disordering in the cell and loss of electric contact resulting in accelerated capacity and power fade (Birkl et al., 2017). When recharging at maximum charge rate in winter conditions for the candidate warship, load shed logic of non-essential loads to be applied to the generators on each switchboard as the recharge power requirement exceeds the N-1 power reserve condition.</td>
</tr>
</tbody>
</table>

6.6 Concluding system recommendations

The aim of this chapter was to investigate the ability of the candidate battery ESS, DC/DC converter and control system to meet the needs of LDEW loads using modelling tool 3, a dynamic modelling tool,
described in chapter 4. Section 6.2 discussed the context and presented justification of the two-part investigation presented in this chapter. The results of the ability of the battery ESS to perform the LDEW power supply function were presented in section 6.3 for BoL conditions. The first part of the investigation was then used as a benchmark for the second part in section 0 that explored the ability of the battery ESS to perform the LDEW power supply function when the internal resistance has increased and the storage capacity has degraded. For both investigations, implications and consequences of the results was offered in section 6.5. Using the modelling approach developed in this research, this chapter concludes with recommendations that this research has contributed to the field when using battery ESS to power LDEW loads in warship hybrid power systems.

6.6.1 Recommendations

The recommendations in this sub-section are offered for consideration by the naval design authority responsible for integrating the LDEW, battery ESS and converter with the candidate warship power system, as well as, the battery ESS designer.

From the perspective of the naval design authority it is not recommended that the candidate ESS in a four string arrangement, as used in this research, be considered to power LDEW loads with 2 MW peak pulse power. This is due to the limited useable SoC range to meet the LDEW demands. However, based on the results of this research it is recommended that the limit be placed at 1.75 MW peak LDEW power. This permits an SoC envelope of operation, that is at worst case, between 75% and 90% of the useable storage capacity. This SoC envelope allows the ESS to meet the four minute operating requirement when the internal resistance increases by 19% and storage capacity decreases by 10% compared to BoL conditions. Conversely, the 2 MW LDEW peak power scenario limits the useable battery ESS range to between 89% and 90% when the battery ESS is under the same degradation condition.

The results of the QPS investigation determined the output voltage deviations at ESS BoL and when degraded. If a 2 MW LDEW load with 25 ms time to full radiant intensity is a mandatory requirement for the candidate warship, it is recommended to the naval design authority that the limits of IEEE 1709 be challenged for the system presented in this research. This is because the power system voltage fluctuations are less than 0.75% above the 10% tolerance limit through the life of the candidate ESS. Chapter 7 discusses future work recommendations that could potentially be explored to limit the deviations using alternative control systems.

When considering the recharging requirement for the battery ESS for two successive engagements in the candidate power system it is recommended to the naval design authority that load shed logic be included in the power management system to ensure the maximum recharge power can be provided to the battery ESS whilst satisfying the N-1 power reserve safety condition during winter and tropical climate conditions.

The model developed in this work assumes an effective cooling system with the capacity to maintain ambient temperatures in the ideal operating range of 20-25°C. It is recommended to the battery ESS design authority that for LDEW load applications, a minimum of 27% heat removal margin be added to remove the heat generated by cells during the BoL condition as presented in Fig. 6-18, before the detailed design
phase is conducted to include proximity effects of the cells under LDEW operation. This is a key finding of this novel research into how the battery ESS performs when powering the LDEW under degraded conditions.
Chapter 7   Conclusions and recommendations

7.1   Summary

This chapter provides answers to the three research questions that were posed in chapter 1. The research questions pertain to the performance of a battery ESS and the power system when integrated as part of the power and propulsion system with hybrid configuration. Following a literature review into the state-of-the-art, integration and operation of battery ESS in ship power systems, it was concluded that the key operating modes of interest for warship hybrid power systems are power reserve, load levelling and LDEW power supply. Based on the subsequent critical review of battery ESS of these three operating modes in warship hybrid power systems, it was concluded that understanding and knowledge was lacking in three key aspects:

1) **Power reserve.** On the quantification of power system performance when using battery ESS as power reserve, cognisant of warship operating profiles, and power system operating configurations during different operating states.

2) **Load levelling.** On the electrical and fuel consumption performance of a candidate warship AC power system while under load levelling.

3) **LDEW power supply.** On the interfacing of the power electronic converter and control system of the battery ESS with the LDEW. Further, on the transient impact of the LDEW on the battery ESS.

To advance knowledge in the field on these three aspects, three requirements were identified. First, the requirement for a model capable of exploring steady state performance of a warship hybrid power system over its operating profile when a battery ESS is operating as a power reserve. Second, the requirement for a model to explore hybrid power system electrical performance when a battery ESS is operating to load level DG sets under quasi-steady state conditions. The final requirement was the design and development of a novel battery based LDEW power system model capable of exploring the impact of continuous LDEW firing for up to four minutes on operation of the battery ESS and power system QPS.
Conceptual understanding of how an ESS is integrated with a warship hybrid power system and how the battery ESS functions in each operating mode was then identified. This was conducted by selecting and justifying a candidate warship hybrid power and propulsion system for use as a case study and describing the ship operating states and corresponding power system configurations. Based upon the conclusions derived from the literature review, a candidate ESS using Li-ion NMC battery technology was selected and justified. This is significant since a clear understanding of the performance of this specific battery ESS technology when powering LDEWs in warship power systems is not yet known. The candidate battery ESS was integrated with the main distribution bus of the warship power system via a bi-directional DC/AC power converter and transformer. This design of integration interface with the main switchboard was selected because by its location it offers the flexibility to provide power to both propulsion and ship services. An assessment of the different control methods of bi-directional converters was carried out. It was proposed that the converter should be controlled to be a grid-supporting VSC as this mode of control permits the ESS to function as the sole power source if all the generators are off-line. The LDEW was co-located with the ESS via a DC/DC power converter. This arrangement was selected to decouple the LDEW transients from the main distribution system. Theoretical and conceptual analysis of the interaction between the LDEW, DC/DC converter and battery ESS was developed and this allowed understanding of the conceptual impact of the LDEW on the battery ESS.

To investigate and analyse the performance of the battery ESS and ship power system in each of the three operating modes, three modelling tools were developed based on the conceptual understanding developed in chapter 3. In chapter 4, description of each of the three modelling tools was presented, including justification of the component model parameters, assumptions and limitations. The components of the modelling tools were subjected to a verification and validation process to ensure credibility of the results generated in the subsequent investigations of this research. The battery cell model developed from experimental test data was validated at battery module level. This is significant because the battery modules are certified for use in commercial marine applications. Consequently, results of the novel investigations conducted in this research inform the battery ESS designers of battery ESS performance under naval application scenarios. Therefore, the impact to designers is that the results of this research help de-risk the technology for naval application.

In chapter 5, modelling tool 1 was employed to quantify and explore steady-state power system performance when the battery ESS is operated in power reserve mode. The first investigation in chapter 5 demonstrated the achievement of fuel and exhaust GHG emission savings in excess of 35% and 40% respectively compared to conventional warship operating practice. Modelling tool 2 was employed to investigate the electrical performance of the power system, fuel and exhaust GHG emissions during load levelling in quasi-steady state conditions. The load levelling investigation in chapter 5 determined fuel performance and VSC voltage droop setting limits to ensure voltage fluctuations were adequately minimised.

In chapter 6, modelling tool 3 was employed to explore the system QPS response, transient response and operating envelope of the battery ESS, when meeting the load requirements commensurate with LDEWs. This was conducted when the battery ESS is at beginning of life and under degraded conditions. Results
and observations were presented when powering the LDEW using the battery ESS. This investigation achieved results that quantified the QPS performance of the system, the SoC operating envelope of the battery ESS, the heat generation of the system and recharge implications on the candidate warship power system under LDEW operation. It was shown that the control system designed for the DC/DC converter could limit the voltage fluctuations under LDEW activation to within the tolerances mandated by IEEE 1709. However, the 2 MW LDEW load scenario with a 25 ms time to full radiant intensity proved to be an exception.

A summary of the contributions to knowledge, recommendations to battery ESS designers and the naval power system design authority are provided in the following section. The impact of future trends in battery technology on the research findings are then observed. Areas of future work to develop further understanding of the research problems on battery operation in warship hybrid power systems are then suggested.

### 7.2 Contributions to knowledge

The contributions to knowledge as a result of this research will be detailed in response to the research questions detailed in chapter 1, and in the context of the operating modes of the ESS. The operating modes being power reserve, load levelling and LDEW power supply.

This work acknowledges the contributions to the literature by S.-Y. Kim et al. (2015), Radan et al. (2016) and Southall and Ganti (2018) to the use of ESS to operate as power reserve for generator sets. This research advanced the power reserve methodology further by including the battery ESS interface and efficiency characteristics of the power sources to increase the fidelity of the system characteristics and therefore the accuracy of the results using the power reserve method. The analytical modelling tool detailed in section 5.2.2 was used to ascertain results that aimed to answer the first research question posed, which was:

*What are the implications, consequences and constraints on warship hybrid power system performance when a battery ESS operates as power reserve?*

From Table 5-15, the following research findings can be proffered in response to research question 1:

- The power and energy specification of the ESS is determined by the operational specifications required to satisfy the LDEW power supply mode. The consequential power rating of the ESS is found to be sufficient to increase the generator load margins to 80% in split bus configuration of the switchboards and 100% in closed bus configuration.

- The use of the ESS specified in this research to operate in power reserve mode has the potential to reduce the candidate ship’s fuel consumption by up to 36%.

- The use of the ESS specified in this research to operate in power reserve mode has the potential ability to reduce the candidate ship’s exhaust GHG emissions footprint by 40%. This is a significant reduction for a naval warship, to provide context for comparison, the IMO world shipping GHG emissions reduction target is 50% by 2050, compared to 2008 (IMO MEPC, 2018).
The second research question posed was:

_What are the implications, consequences and constraints on power system performance under load levelling conditions whilst ensuring acceptable levels of QPS?_

In chapter 5, electrical power system performance for the candidate warship was investigated using modelling tool 2, the following research findings can be proffered in response to answering research question 2:

- It was found that when an ESS is interfaced with a conventional LV AC warship power system via a VSC, controlled as a grid-supporting converter, circulating reactive power manifests when operating in parallel with a DG, which could result in overcurrent damage of the VSC under highly dynamic conditions.

- Under quasi-steady state conditions frequency and voltage QPS were maintained within the NATO STANAG 1008 standard for AC LV electrical power systems. The voltage droop setting of the VSC should not exceed 2%. This is important as the naval design authority will need to be aware of the voltage fluctuations that other loads, such as propulsion and ship services, may be exposed to under load levelling conditions.

- There is potential to reduce fuel consumption and exhaust GHG emissions by up to 22.9% and 36.3% respectively compared to conventional practice during load levelling.

The third research question posed was:

_What are the implications, consequences and constraints on power system and battery ESS operation to ensure acceptable levels of QPS and prevent damage to the battery ESS under LDEW operation?_

Acknowledging the key previous contributions from the simulation based research of Gattozzi et al. (2015) and hardware in the loop testing of Langston et al. (2019), identified in chapter 2, this research has advanced knowledge in the field of using battery ESS when operating as an LDEW power supply. This is the case because the referenced contributions did not present the battery characteristics, power electronic interface and control system in sufficient detail to adequately discuss the battery limitations during operation of an LDEW. In chapter 6, the ability of the candidate NMC based battery ESS was investigated to supply the requirements commensurate with future LDEW loads using modelling tool 3, the following research findings can be proffered in response to research question 3 and advance on the previously published knowledge:

- To remain within recommended QPS practice for DC loads, the time to full radiant intensity of the LDEW should be greater than 25 ms, as departure from the recognised limits of standard IEEE 1709 could occur, triggering protection relays of other equipment.

- The 2MW LDEW load has significant consequences for the useable SoC range of the candidate battery ESS in this research. For the best case, the initial SoC range is 7% of useable storage capacity due to the short rise time and magnitude of the LDEW load.
To account for degradation of the battery ESS, a heat loss margin from the ESS needs to be included in the cooling system design. It was found there was a 27% increase in heat generated due to degradation of the battery ESS through cyclic and calendar aging compared to beginning of life conditions, when under LDEW loading.

The findings of this research have given rise to specific recommendations to battery ESS designers and the naval power system design authority when integrating battery ESS with warship hybrid power systems to meet the demands of power reserve, load levelling and LDEW power supply operating modes.

7.3 Recommendations to battery ESS designers and the naval power system design authority.

The approach of this research has contributed the following recommendations to battery ESS designers and the naval power system design authority when battery ESS are integrated with warship hybrid power systems to operate in power reserve, load levelling and LDEW power supply modes. The aim of these recommendations is to ensure the battery is correctly specified for the operating mode requirements that assures the performance of the power system to meet the demands of the LDEW and other power system consumers. Second that the battery ESS functions within its acceptable operating envelope and that damage to the cells in the system is minimised. The resulting recommendations are as follows:

1) When operating in load levelling mode, the maximum VSC voltage droop setting is recommended to be 2%. This is significant to minimise voltage fluctuations on the main busbar of the candidate power system.

2) When utilising the candidate battery ESS to operate as power supply to future LDEW loads, that the power limit of the LDEW load is 1.75 MW. The significance of this is to permit a practical initial SoC operating envelope when powering the LDEW, which is from 66.7% to 90% SoC at BoL conditions.

3) If a 2 MW capable LDEW with 25 ms time to full radiant intensity is required by the warship, it is recommended that the naval power system design authority challenge or make provision to mitigate for exceeding the ±10% voltage tolerance limits of IEEE 1709. This is because the recorded deviations are less than 0.75% outside this tolerance limit.

4) The ESS cooling system design should be supplemented with a 27% heat loss margin for LDEW operation to account for cell degradation. This is significant to ensure that the battery cells in the ESS do not reach excessive temperature levels that could cause further degradation and/or prevent the ESS from being able to power the LDEW.
7.4 Observations on the impact of future trends in battery technology on research findings

This research has applied state of the art Li-ion NMC based battery technology used in commercial marine settings, to address the dynamic challenges being faced by warship power systems under LDEW operation and also routine operations. This subsection observes how the future trends in battery cell technology detailed in Chapter 2 could impact the research findings. The candidates for future battery chemistries applicable to this research were categorised into near term (0-5 years), medium term (5-10 years) and long term (>10 years).

7.4.1 Near term (0-5 years)

In the near future NMC 811 will become an important chemistry for battery technology in transport, moving beyond the capability of the typically used NMC 532 and NMC 111. In the context of the research findings, this chemistry could unlock the capability to operate the 2 MW LDEW over a wider state of charge operating envelope, potentially increasing the feasibility of using NMC based battery systems for 2 MW LDEW. The chemistry has superior discharge rate capability due to higher electronic conductivity. This correlates to reduced internal resistance of the cell and therefore a lower voltage drop across the cell terminals under pulsed loading, therefore a higher number of shots will be required to breach the voltage cut-off. The reduced voltage drop across the cell terminals under LDEW loading could improve the QPS on the output of the boost converter, as the control action required from the inner current loop controller is reduced. The higher specific energy of NMC 811 compared to NMC 111 chemistry, could permit an increased number of LDEW shots for the same volumetric and mass footprint as the system used in this research. Alternatively, the number of strings could be reduced.

From the perspective of power reserve during routine operations, the improved power rating of an ESS with NMC 811 chemistry, subject to the limits of the power conversion equipment and fault level of the power distribution system, could increase the generator loading margins further. Resulting in further reductions in the candidate ship’s fuel consumption and exhaust GHG emissions.

Adopting NMC 811 could improve reduce the magnitude of voltage oscillations on the main distribution bus due to smaller internal resistance of the cells. The smaller resistance will maintain a more stable voltage input to the VSC for a given load, reducing the dynamics of the VSC control system action.

7.4.2 Medium term (5-10 years)

If the electrolyte decomposition challenges can be overcome with high voltage spinel, this chemistry has potential benefits of higher specific energy and higher rate capability than NMC 811. The high voltage spinel cathode compound comprises Nickel, Manganese and Oxygen, with a theoretical specific energy 66% higher than that of NMC-811. In a similar manner to NMC 811, high voltage spinel chemistry could increase the state of charge operating envelope of the battery system and limit the output voltage deviations under the LDEW loading conditions. Further the magnitude of the voltage oscillations on the distribution bus under load levelling could be reduced.
7.4.3 Long term (>10 years)

The next generation of battery technology includes Li-sulphur (Li-S), Li-air and solid state batteries. These battery chemistries have a number of barriers to commercialisation, which limits the application to each of the operating modes explored in this research.

The maximum practical specific energy of Li-S, 600 Wh kg\(^{-1}\) (Cano et al., 2018), is an attractive characteristic, however Li-S cells suffer from low cycle life, low ionic conductivity and therefore power capability, making them unattractive for repetitive pulsed loading in LDEW applications. Due to the low ionic conductivity Li-S technology may not be able to achieve the same discharge rates required to meet the LDEW rise time of the load.

Due to the maturity of Li-air battery technology, reliable power density and energy efficiency estimates are not available (Cano et al., 2018). However, it is expected that these characteristics would be poorer than the previously discussed chemistries due to sluggish oxygen kinetics at the air electrode (Christensen et al., 2011; Manthiram and Li, 2015) and therefore based on the literature, the application of this technology to LDEW power supply is currently not appropriate.

Until recently, solid state batteries have been inhibited by high internal resistance and low power density due to low-ionic conductivity, inferior to that of NMC (J. G. Kim et al., 2015; Kim et al., 2019). However technological advancements have shown increased C-rate capability is possible (<1500 C), which shows the potential for solid state batteries to become superior to NMC cells with liquid electrolytes (6 C in this research). Solid state technology is an attractive future alternative, as it could avoid the safety concern of electrolyte leaks or fires. The operating temperature characteristics could, however increase the auxiliary cooling load of the candidate power system as cells would require operating temperatures up to 100°C to achieve these high discharge rates (Ding et al., 2019). The challenge of this technology may arise in the system integration, the cells have higher internal resistance compared to liquid electrolyte counterparts, therefore the voltage drop across the battery terminals could be greater under LDEW loading and load levelling conditions, therefore voltage deviation magnitude could increase compared to the results reported in this research. Therefore the control systems for the DC/DC and VSC used in this research may have to be adapted in any future work involving solid state battery systems.

7.5 Recommendations for future work

Four key areas for future work have been identified throughout this research. These proposed areas aim to provide a more complete understanding of the problems investigated. These are detailed in the following sub-sections.

7.5.1 Explore alternative control strategies for the VSC converter

The bi-directional DC/AC power converter used to interface the battery ESS with the warship power system was controlled as a grid-supporting VSC. As discussed in section 3.6.2, this type of control for the converter was selected to allow the active and reactive power to be controlled. Furthermore, this type of control allows
the battery ESS to act as the sole power source if the generators are off-line. However, as the results of this research in section 5.5 have demonstrated, the control system used in this research was capable of accurately controlling active power and frequency, but accurate control of the reactive power and voltage proved unpredictable, and circulating currents manifested during parallel DG and battery ESS operation. It is therefore suggested that alternative control strategies be explored to determine if a superior strategy is available to limit or eliminate the circulating currents exhibited in the results in section 5.5. A suggested method to address this is a transitional control system. This could be developed and implemented in modelling tool 2. This control system could transfer between grid-feeding mode when paralleled with DGs, to grid-supporting mode when there is no reference from the DGs on the main distribution network. This would mean that in grid-feeding mode the VSC converter uses the DGs as a reference and follows the voltage and frequency set by the ships generators, therefore droop control is not required (Bouzid et al., 2015).

7.5.2 Thermal response of the system to LDEW loading

The battery model employed in this research does not include a thermal component for the cell, module and string of the battery ESS. However, the heat generation from the cells during LDEW power supply were measured during the investigations conducted in chapter 6, which have provided important information on the heat generation performance under LDEW operation which can be applied to conduct studies at module level. It is recommended that the exothermic response of the cells during LDEW operation are explored using experimental testing when configured in a module to compare to the results provided in this research in chapter 6. Experimental module testing will capture the proximity effects of the cells on thermal behaviour of the system. This will allow the investigation of the thermal impact of LDEW loading on the battery ESS and provide a more complete understanding of the system response to LDEW operations. Experimental testing could permit a multi-physics model to be developed and further simulation based testing to be conducted. This will inform battery ESS designers whether the cooling system of the candidate battery ESS simulated in this research is capable of maintaining the temperature conditions of the cells in the ESS within the limits that prevent over-temperature safety mechanisms from being triggered under LDEW operations.

7.5.3 Further develop the DC-DC converter controller

This research has developed a control system for a single stage DC/DC boost converter that is capable of acting to mitigate adverse voltage QPS conditions under LDEW operations. This was achieved by including a feedforward term in the inner loop of the current mode control system. Details of the large output current required by the LDEW and terminal voltage drop of the battery ESS are fed forward to the fast acting inner loop to adjust the converter switching duty cycle to control the inductor current of the converter. The control system is capable of maintaining QPS to within ±10% of rated voltage as required by IEEE 1709, except for when the LDEW peak power is 2 MW, with 25 ms time to full radiant intensity. In this case the maximum deviations were 0.75% higher than the voltage limit. It was recommended that the naval power systems design authority challenge the QPS standard for an exception due to the relatively small deviation above the transient limit. If this is not acceptable, further development of the controller could be examined.
The first recommendation in this case is to explore the use of variable gains in the PI controllers of the control system developed in this research. The use of variable gains based on the voltage of the battery ESS and load resistance could improve the outer loop controller response, as shown by Kasicheyanula and John (2019) for supercapacitor based systems. An alternative control strategy that may be explored is model predictive control. Model predictive control strategies predict suitable control signals to minimise a cost function. The cost that would be minimised in the case of this research is the voltage transient on the output of the LDEW. Such strategies are being investigated for converter control during pulsed loading in naval power systems (Mardani et al., 2019).

7.5.4 Explore engine emissions under transient loading in the context of battery load levelling

The exhaust GHG emissions of the diesel engine in this research were determined using a quasi-steady state approximation. This was achieved using a combination of the engine operating point within its operating envelope and the IMO emission factor method, the method was used in both the power reserve and load levelling operating mode investigations. The engine model employed in the load levelling investigation is a transfer function representation of the governor controller and actuator, and an engine time delay. While this has provided an important estimation of emissions savings, the disadvantage of the comparison made in this research to the baseline candidate warship is that the emissions formed during transient acceleration of the engines during the load profiles simulated are not quantified. Transient accelerations of the engine compared to constant loading could incur the cost of higher exhaust valve temperatures, rate of change of exhaust valve temperature and air excess ratio, which could contribute to higher smoke and particulate matter emissions (Geertsma, 2019). An example that evidences transient load impact on engine emissions has been demonstrated in automotive diesel engines. Ko et al., (2017) showed that NOx emissions during the world-harmonised light-duty vehicle test cycle, with increased acceleration profile, are much higher than the current Euro 6 standard of 80 mg/km, which is assessed in the new European driving cycle with more constant loading. It is recommended that a more complex dynamic model of the diesel engine is developed, in combination with validating experiments. The model then incorporated with modelling tool 2 to predict exhaust GHG emissions due to the local effects in the combustion process of engine accelerations under transient load conditions.
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Whitelegg, I., 2016b. On the integration of electromagnetic railguns with warship electric power systems. University College London.


References


Appendix A. Corvus Energy Ltd confirmation letter

This appendix contains confirmation of the authenticity of the data provided by Corvus Energy Ltd, with which the battery ESS simulation model was developed and validated for use in this research.

Corvus Energy
#100 – 13551 Commerce Parkway
Richmond, BC
V6V 2L1

October 7, 2019

Dear Sir/Madam,

I can confirm that Corvus Energy Ltd have supplied Luke Farrier with cell and module level experimental testing data with which to conduct his PhD research. I can confirm that the parameters of the ESS used by Luke Farrier in his research are representative of the Orca System developed by Corvus Energy Ltd. Each Orca System module comprises twenty-four 4.2 V lithium NMC cells rated at 64.4A in a 12 series, 2 parallel arrangement. The cells are capable of a peak discharge rate of 6C and peak charge rate of 3C. The cell nominal voltage is 3.7 V and the cut-off voltage is 2.6 V.

The following experimental data sets for the lithium NMC cell used in the Orca System were supplied to Luke Farrier to conduct his research:

1. Cell current and voltage response during pulsed discharge data from 100% to 0% SoC at 5% intervals, at 0.5C rate.
2. Cell current and voltage response during constant current discharge 0.5C, 1C, 3C and 6C tests.
3. Cell current and voltage response during constant current-constant voltage charging at 0.5C, 1C and 3C tests.

The following experimental data sets for the Orca System module were supplied to Luke Farrier to conduct his research:

1. Module 2C charge and discharge voltage and current profile.
2. Module 4.5C discharge voltage and current profile.

I can confirm that Luke Farrier has validated his simulation model against the data supplied to him as detailed above in points 1, 2 and 3 for the cell experimental testing and points 1 and 2 for the module experimental testing.

Yours faithfully,

[Redacted name]
Systems Engineer

www.corvusenergy.com
Appendix B. Simulink battery ESS model schematic

This appendix contains the battery ESS model schematic as built in the Simulink® modelling environment. This model is described in detail in chapter 4.

Fig. B-1. Battery ESS Simulink® model schematic with 22 modules in each string and 4 parallel strings

Fig. B-1 shows the battery ESS with the 128 Ah module representation on the right. The series and parallel multiplier is set up in the middle to scale the modules to represent the string and number of strings in the ESS respectively. The right hand side of the schematic shows the assumed 1 mΩ contact resistance with the system ESS data logging and terminals of the ESS to the system level simulation.

Fig. B-2. Module level Simulink® model schematic with 12 series cells and 2 parallel sets of cells

Fig. B-2 shows the 128 Ah module schematic of the battery ESS. From left to right, first the schematic shows the Li-ion NMC cell model. Second the contact resistance and series and parallel calculations to represent the 12s2p module configuration. Finally the module positive and negative connections to the string level schematic in Fig. B-1.
Fig. B-3. Li-ion NMC Cell level Simulink® model schematic of the Thévenin equivalent circuit model

Fig. B-3 shows the Thévenin equivalent circuit model described in detail in chapter 4. Top left of Fig. B-3 shows the parameter maps and SoC hard operating minimum limit of 20%. The top of the schematic is the implementation of the coulomb counting method in (4.1). The bottom left of the schematic is the open circuit voltage followed by series resistance, RC pair branches with variable resistor and capacitor sub-systems. The cell terminals are bottom right of the schematic.

Fig. B-4. Variable (a) resistor and (b) capacitor Simulink® model schematic

As there is no variable resistor or capacitor in the Simulink® block library, these were modelled using controllable current and voltage sources using (B.1) and (B.2) respectively as shown in Fig. B-4(a) and (b). Where $R_{\text{var}}$, $v_{\text{res}}$, $i_{\text{res}}$ are the variable resistance, voltage and current of the $i$th branch resistor respectively. $C_{\text{var}}$, $v_{\text{cap}}$ and $i_{\text{cap}}$ are the variable capacitance, voltage and current of the $i$th branch capacitor respectively.
\[ i_{res} = \frac{v_{res}}{R_{var}} \]  
(B.1)

\[ v_{cap} = \frac{1}{C_{var}} \int i_{cap}(t) \]  
(B.2)
Appendix C. Parameter extraction code

This appendix contains the automated parameter extraction code used in this research to process the discharge data provided by Corvus Energy as detailed in section 4.6.2.

```matlab
%% Automated parameter extraction file 

% prerequisites
Icp = 32; % current pulse amplitude (A)
tcp = 360; % current pulse period (s)
t22=600; % time instances (s)
t21=240;
t12=12;
t11=0;

%% preprocessing to define time windows of PD experiment
Time_sample = seconds(Time);
TF = islocalmin(VDC,'MinSeparation',seconds(2150),'SamplePoints',Time_sample);
% find local minima of the pulses
[rows] = find(TF); % select the time step at which the local minima occurs
zero_t = rows(1:20); % define the rest windows for analysis
v_zero_t = VDC([zero_t+1]); % identify the corresponding initial voltage of the rest period

% define time windows of each rest period and select voltages from test data
v_12 = VDC([t_12]);
v_21 = VDC([t_21]);
v_22 = VDC([t_22]);
t_end = zero_t + 1800; % array of end (30 minute rest periods during PD test)

%% Processing data to extract parameters

% select all instances of uTt11, uTt12 based on v_zero_t and zero_t arrays.
Then find Tau 1, Tau 2, U1 and U2 for each row of the array, then calc Ri and Ci

% Second window to calc Tau 2, u2, U2. First select subsets of voltage and time data
n = 20; % number of current pulses
V = nan(1800,n); % define matrices to fill
T = nan(1800,n);

v_22_mod = zero_t + t22;
VOCV_mod = rot90(VOCV(2:21));
VOCV_mod = repmat(VOCV_mod,1800,1);

% Select voltage from experimental data
for i=1:n
    V(:,i) = VDC(zero_t_mod(i):t_end(i));
    T(:,i) = Time(zero_t_mod(i):t_end(i));
end
T1 = T - repmat((zero_t)',1800,1);
VOCV_mod = rot90(VOCV(2:21));
VOCV_mod = repmat(VOCV_mod,1800,1);
```
% invert to find transient voltage response
uT = VOCV_mod - V;

uTt21 = nan(20,1);
uTt22 = nan(20,1);

% determine the second branch uTt11, uTt12
for i=1:n
    uTt21(:,1) = uT(t21,:);
    uTt22(:,1) = uT(t22,:);
end

% calculate second branch time constant based on uTt11 and uTt12
den = log(uTt21./uTt22);
num = repmat(t22-t21,20,1);
Tau2 = num./den;

% calculate predicted second branch transient circuit voltage response during rest periods
U2 = uTt21.*exp(repmat(t21,20,1)./Tau2);
U2 = repmat(rot90(U2),1800,1).*exp(-T1./(repmat(rot90(Tau2),1800,1)));

%% first window-
% preprocess to remove the second long time constant voltage from the experimental data
uT_mod = uT - uT2;

% calculate first window time constant
uTt11 = nan(20,1);
uTt12 = nan(20,1);

m = 20;
for i=1:m
    uTt11(:,1) = uT_mod(t11+1,:);
    uTt12(:,1) = uT_mod(t12+1,:);
end
den = log(uTt11./uTt12);
num = repmat(t12-t11,20,1);
Tau1 = num./den;

U1 = uTt11.*exp(repmat(t11,20,1)./Tau1);
U1 = repmat(rot90(U1),1800,1).*exp(-T1./(repmat(rot90(Tau1),1800,1)));

% transient circuit voltage response for both windows
uT_tot = uT1 + uT2;

%% Resistance and capacitances as function of SOC 
% calculate R1 and C1
R2 = U2./(Icp*(1 - exp(-tcp./Tau2)));
R1 = U1./(Icp*(1 - exp(-tcp./Tau1)));
C2 = Tau2./R2;
C1 = Tau1./R1;
Appendix D. Battery model parameter maps

Fig. D-1 shows the parameter map for the internal resistance of the cell and the polynomial function used in the simulation model of the battery cell. The top plot shows the extracted $R_0$ values from the experimental data in black, against the polynomial function in blue. The lower plot shows the developed error using residuals. Values below 8% SoC were excluded to maximise the goodness of fit in the useable SoC range of 20-90% SoC. The polynomial function and goodness of fit information is provided below in Table D-1. Table D-1 includes the goodness of fit of the polynomial to the extracted parameters, where SSE is the sum of squares due to error, $R^2$ is the statistical measure of the correlation between the polynomial and the predicted values from the parameter map. The RMSE is the root mean squared error, it estimates the standard deviation of the random component of the data.

Table D-1. Polynomial function used in the simulation model for $R_0$ and corresponding goodness of fit data

<table>
<thead>
<tr>
<th>Polynomial function for $R_0$ where $x$ is SoC</th>
<th>Coefficients</th>
<th>SSE</th>
<th>$R^2$</th>
<th>RMSE</th>
</tr>
</thead>
<tbody>
<tr>
<td>$f(x) = p_1 x^5 + p_2 x^3 + p_3 x^2 + p_4 x + p_5$</td>
<td>$p_1 = 7.393e-11$</td>
<td>$p_2 = -1.982e-08$</td>
<td>$p_3 = 1.863e-06$</td>
<td>$p_4 = -7.119e-05$</td>
</tr>
</tbody>
</table>
Fig. D-2 shows the parameter map for the internal resistance of the cell and the polynomial function used in the simulation model of the battery cell. The top plot shows the extracted $R_1$ values from the experimental data in black, against the polynomial function in blue. The lower plot shows the developed error using residuals. Values below 5% SoC were excluded to maximise the goodness of fit in the useable SoC range of 20-90%. The polynomial function and goodness of fit information is provided below in Table D-2.

![Fig. D-2](image-url)

Table D-2. Polynomial function used in the simulation model for $R_1$ and corresponding goodness of fit data

<table>
<thead>
<tr>
<th>Polynomial function for $R_1$ where $x$ is SoC</th>
<th>Coefficients</th>
<th>SSE</th>
<th>$R^2$</th>
<th>RMSE</th>
</tr>
</thead>
<tbody>
<tr>
<td>$f(x) = p_1 x^4 + p_2 x^3 + p_3 x^2 + p_4 x + p_5$</td>
<td>$p_1 = -5.534e-09$</td>
<td>$p_2 = 8.766e-07$</td>
<td>$p_3 = -3.928e-05$</td>
<td>$p_4 = -7.119e-05$</td>
</tr>
</tbody>
</table>
Fig. D-3 shows the parameter map for the internal resistance of the cell and the polynomial function used in the simulation model of the battery cell. The top plot shows the extracted $R_2$ values from the experimental data in black, against the polynomial function in blue. The lower plot shows the developed error using residuals. Values below 5% SoC were excluded to maximise the goodness of fit in the useable SoC range of 20-90%. The polynomial function and goodness of fit information is provided below in Table D-3.

![Plot showing parameter map for internal resistance of the cell and polynomial function](image)

Table D-3. Polynomial function used in the simulation model for $R_2$ and corresponding goodness of fit data

<table>
<thead>
<tr>
<th>Polynomial function for $R_2$ where $x$ is SoC</th>
<th>Coefficients</th>
<th>SSE</th>
<th>$R^2$</th>
<th>RMSE</th>
</tr>
</thead>
<tbody>
<tr>
<td>$f(x) = p_1 x^4 + p_2 x^3 + p_3 x^2 + p_4 x + p_5$</td>
<td>$p_1 = 1.553 \times 10^{-10}$</td>
<td>$1.123 \times 10^{-05}$</td>
<td>0.306</td>
<td>0.002444</td>
</tr>
</tbody>
</table>

Fig. D-3. Cell parameter map of second RC branch resistance $R_2$ and polynomial function in blue, residual plot of the polynomial against predicted $R_2$ from experimental data
Fig. D-4 shows the parameter map for the internal resistance of the cell and the polynomial function used in the simulation model of the battery cell. The top plot shows the extracted $C_1$ values from the experimental data in black, against the polynomial function in blue. The lower plot shows the developed error using residuals. The polynomial function and goodness of fit information is provided below in Table D-4.

![Cell parameter map of first RC branch capacitance $C_1$ and polynomial function in blue, residual plot of the polynomial against predicted $C_1$ from experimental data](image)

Table D-4. Polynomial function used in the simulation model for $C_1$ and corresponding goodness of fit data

<table>
<thead>
<tr>
<th>Polynomial function for $C_1$ where $x$ is SoC</th>
<th>Coefficients</th>
<th>SSE</th>
<th>$R^2$</th>
<th>RMSE</th>
</tr>
</thead>
<tbody>
<tr>
<td>$f(x) = p_1 x^4 + p_2 x^3 + p_3 x^2 + p_4$</td>
<td>$p_1 = 0.4076$</td>
<td>$3.405e+09$</td>
<td>0.8644</td>
<td>4157</td>
</tr>
<tr>
<td></td>
<td>$p_2 = -69.42$</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td></td>
<td>$p_3 = 3365$</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td></td>
<td>$p_4 = -7.183e-05$</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td></td>
<td>$p_5 = 1756$</td>
<td></td>
<td></td>
<td></td>
</tr>
</tbody>
</table>
Fig. D-5 shows the parameter map for the internal resistance of the cell and the polynomial function used in the simulation model of the battery cell. The top plot shows the extracted \( C_2 \) values from the experimental data in black, against the polynomial function in blue. The lower plot shows the developed error using residuals. The polynomial function and goodness of fit information is provided below in Table D-5.

![Graph showing parameter map and polynomial function](image)

Table D-5. Polynomial function used in the simulation model for \( C_2 \) and corresponding goodness of fit data

<table>
<thead>
<tr>
<th>Polynomial function for ( C_2 ) where ( x ) is SoC</th>
<th>Coefficients</th>
<th>SSE</th>
<th>( R^2 )</th>
<th>RMSE</th>
</tr>
</thead>
<tbody>
<tr>
<td>( f(x) = p_1 x^4 + p_2 x^3 + p_3 x^2 + p_4 x + p_5 )</td>
<td>( p_1 = -0.2059 ) ( p_2 = 46.92 ) ( p_3 = -3309 ) ( p_4 = 8.182e+04 ) ( p_5 = 4.147e+05 )</td>
<td>1.832e+13</td>
<td>0.5262</td>
<td>3.058e+05</td>
</tr>
</tbody>
</table>
Fig. D-6 shows the parameter map for the open circuit voltage of the cell and the polynomial function used in the simulation model of the battery cell. The top plot shows the extracted OCV values from the experimental data in black, against the polynomial function in blue. The lower plot shows the developed error using residuals. The polynomial function and goodness of fit information is provided below in Table D-6.

![Cell parameter map of open circuit voltage $u_{OC}$ and polynomial function in blue, residual plot of the polynomial against predicted $u_{OC}$ from experimental data](image)

Table D-6. Polynomial function used in the simulation model for $u_{OC}$ and corresponding goodness of fit data

<table>
<thead>
<tr>
<th>Polynomial function for $u_{OC}$ where $x$ is SoC</th>
<th>Coefficients</th>
<th>SSE</th>
<th>$R^2$</th>
<th>RMSE</th>
</tr>
</thead>
</table>
| $f(x) = p1x^4 + p2x^3 + p3x^2 + p4x + p5$       | $p1 = -2.755e-08$  
$p2 = 6.394e-06$  
$p3 = -0.0004451$  
$p4 = 8.182e+04$  
$p5 = 3.374$ | 0.01367 | 0.9984 | 0.00835 |
Appendix E. Temperature measurements during 0.5 C pulsed discharge experiment

This appendix details the temperature results during the PD experiments conducted by Corvus Energy. A sketch of the pouch type cell is shown in Fig. E-1, highlighting the temperature measurement locations placed on the cell during each of the three PD experiments in the test chamber. The corresponding temperature response for each experiment is shown in Fig. E-2, showing that the temperature of the cell is maintained at T +2°C during each experiment.

Fig. E-1. Cell illustration with temperature measurement locations during PD experiment

<table>
<thead>
<tr>
<th>Temperature measurement locations</th>
</tr>
</thead>
<tbody>
<tr>
<td>A: Positive tab</td>
</tr>
<tr>
<td>B: Central</td>
</tr>
<tr>
<td>C: Negative tab</td>
</tr>
</tbody>
</table>

Fig. E-2. Temperature measurements by Corvus Energy during PD experiment at (a) 15°C (b) 22°C and (c) 30°C
Appendices

Appendix F. Battery efficiency models

This appendix describes the efficiency models for charge and discharge of the cells in the battery ESS, the process for developing these models was described in chapter 4. Fig. F-1 shows the discharge efficiency map for a single cell. The fitted polynomial function is described in Table F-1. The DoD range used to generate the efficiency map is between 90% and 10% DoD, the goodness of fit data for the polynomial in Fig. F-1 Table F-1 shows strong correlation to the efficiency map with low RMSE and high $R^2$ close to 1.

![Efficiency map](image)

Table F-1. Polynomial function used in the simulation model for discharge efficiency and corresponding goodness of fit data

<table>
<thead>
<tr>
<th>Polynomial function for discharge efficiency where $y$ is discharge current and $x$ is DoD</th>
<th>Coefficients</th>
<th>SSE</th>
<th>$R^2$</th>
<th>RMSE</th>
</tr>
</thead>
<tbody>
<tr>
<td>$f(x,y) = p00 + p10x + p01y + p20x^2 + p11xy + p02y^2 + p30x^3 + p21yx^2 + p12xy^2 + p03y^3$</td>
<td>$p00 = 101.2$; $p10 = -0.1291$; $p01 = -0.01294$; $p20 = 6.141e-05$; $p11 = 6.301e-06$; $p02 = -2.457e-05$; $p21 = -9.957e-07$; $p12 = 4.39e-08$; $p03 = -4.456e-09$;</td>
<td>22.35</td>
<td>0.99</td>
<td>0.11</td>
</tr>
</tbody>
</table>
The charging efficiency map was generated using the same process as the discharge efficiency. The constant voltage charging phase was omitted from the efficiency map as this occurs above 90% SoC. The constant charging phase of the cell is demonstrated in Fig. F-2 where the internal resistance is shown to increase as the battery approaches 100% SoC. The internal resistance, losses and efficiency map are shown in Fig. F-2, Fig. F-3 and Fig. F-4 respectively. The internal resistance is calculated for the constant current charging phase. Negative current in the plots means that the battery is being charged.

![Fig. F-2. Internal resistance map of candidate NMC cell as a function of DoD and charging current during the constant current charging phase](image1)

![Fig. F-3. Candidate NMC cell losses map as a function of DoD and charging current during the constant current charging phase](image2)
Fig. F-4. Efficiency map of the candidate NMC cell during the constant current charging phase as a function of DoD and charging current

Similar to the discharge efficiency curve a polynomial was fitted to the charge efficiency plot, as shown in Fig. F-5 and described by Table F-2. The polynomial shows a strong correlation to the efficiency map with $R^2$ close to 1.

Fig. F-5. Surface fitted to charging efficiency of candidate NMC cell as a function of DoD and charging current
Table F-2. Polynomial function used in the simulation model for charging efficiency and corresponding goodness of fit data

<table>
<thead>
<tr>
<th>Polynomial function for charge efficiency where ( y ) is discharge current and ( x ) is DoD</th>
<th>Coefficients</th>
<th>SSE</th>
<th>( R^2 )</th>
<th>RMSE</th>
</tr>
</thead>
<tbody>
<tr>
<td>( f(x,y) = p00 + p10x + p01y + p20x^2 + p11xy + p02y^2 + p30x^3 + p21yx^2 + p12xy^2 + p03y^3 + p40x^4 + p31y^2x^3 + p22x^2y^2 + p13xy^3 )</td>
<td>( p00 = 97.43 )</td>
<td>21.15</td>
<td>0.99</td>
<td>0.13</td>
</tr>
</tbody>
</table>
Appendix G. Voltage source converter schematics

This appendix contains the VSC model schematic as built in the Simulink® modelling environment. This model is described in detail in chapter 4.

Fig. G-1. Primary control layer schematic with droop control and inner control loops of the VSC

Fig. G-1 shows the primary control layer schematic, a full description of the model can be found in section 4.7. The current and voltage sensors for the inductor and capacitor are shown on the lower left. These signals are transformed to the $dq$ axis, rotating at the power system frequency. The droop controller at top left provides the rotating frequency and voltage $dq$ axis references. At the centre of the schematic is the outer voltage loop PI controllers, these supply the $i_d$ and $i_q$ references to the inner loop current controllers. The PWM voltage reference waveform is then generated to send the pulse command to the IGBTs in the VSC.

Fig. G-2. Droop control system schematic

Fig. G-2 shows the droop controller and voltage reference schematic. A full description of this is provided in section 4.11.2.
Fig. G-3 shows the inner control loop schematic of the VSC, with decoupling feed forward loop. A full description of this is provided in section 4.7.
Appendix H.  LCL filter transfer functions

This appendix derives the transfer functions of the LCL filter described in chapter 4 for the per-phase equivalent circuit in Fig. H-1.

According to Kirchoff’s current law the sum of the currents are;

\[ i - i_c - i_g = 0 \]  \hspace{1cm} (H.1)

From Kirchoff’s voltage law the converter side and grid side voltages can be expressed as;

\[ V_{conv} = i(R + L_1s) + i_c \left( \frac{1}{C_f s} + R_d \right) \]  \hspace{1cm} (H.2)

\[ V_g = -i_g L_g s + i_c \left( \frac{1}{C_f s} + R_d \right) \]  \hspace{1cm} (H.3)

Where \( s \) is the Laplace operator, \( R_i \) is inverter output resistance and \( L_g \) is the sum of output filter inductance, \( L_2 \) and the transformer inductance \( L_{tx} \). From (H.1), expressing (H.2) and (H.3) in terms of impedances:

\[
\begin{bmatrix}
V_1 \\
V_g
\end{bmatrix} =
\begin{bmatrix}
z_{11} & z_{12} \\
z_{21} & z_{22}
\end{bmatrix}
\begin{bmatrix}
i \\
i_g
\end{bmatrix}
\]  \hspace{1cm} (H.4)
The relationship of the inverter output voltage to the grid output current, gives the transfer function of the filter as:

\[ G_d(s) = \frac{i_g}{v} = \frac{z_{21}}{z_{12}z_{21} - z_{11}z_{22}} \]  

\[ G_d(s) = \frac{C_f R_d s + 1}{C_f L_1 L_y s^3 + (L_1 R_d + L_y R_d + L_y R_I)C_f s^2 + (L_1 + L_y + R_d R_d)C_f s} \]  

Current loop transfer function

The current loop transfer function is given by

\[ G(s) = \frac{i(s)}{v(s)} = \frac{1}{L_c s} \frac{(s^2 + R_d C_f Z_{LC}^2 s + Z_{LC}^2)}{(s^2 + R_d C_f w_{res}^2 + w_{res}^2)} \]  

where

\[ Z_{LC}^2 = \frac{1}{L_c C_f} \]  

The passive damping resistor increases losses, decreasing efficiency however, reduces the potential for instability at the resonant frequency of the LCL filter.
Appendix I. IGBT and diode switching energy curves

This appendix describes the IGBT and diode switching energy characteristics derived from the manufacturer data sheet (ABB, 2014) and used in the steady state efficiency model described in Chapter 4.

The characteristic curves of the energy losses that vary with DC collector current are provided in Table I-1, Fig. I-1 and Fig. I-2.

\[ E_{\text{total}} = E_{\text{on}} + E_{\text{off}} \]  
(I.1)

Table I-1. Description of polynomial functions of the IGBT and diode switching and turn off energy functions.

<table>
<thead>
<tr>
<th>Switching energy</th>
<th>Polynomial function for the switching energy where ( x ) is the DC collector current, ( I_c )</th>
<th>Coefficients</th>
<th>SSE</th>
<th>( R^2 )</th>
<th>RMSE</th>
</tr>
</thead>
<tbody>
<tr>
<td>IGBT turn on energy, ( E_{\text{on}} )</td>
<td>( f(x) = p_1 x^2 + p_2 x + p_3 )</td>
<td>( p_1 = 6.83 \times 10^{-8} ) ( p_2 = -6.42 \times 10^{-6} ) ( p_3 = 0.19 )</td>
<td>0.03107</td>
<td>0.99</td>
<td>0.066</td>
</tr>
<tr>
<td>IGBT turn off energy, ( E_{\text{on}} )</td>
<td>( f(x) = p_4 x^2 + p_5 x + p_6 )</td>
<td>( p_4 = 4.87 \times 10^{-8} ) ( p_5 = 2.2 \times 10^{-4} ) ( p_6 = 0.16 )</td>
<td>0.00270</td>
<td>0.99</td>
<td>0.02</td>
</tr>
<tr>
<td>Total IGBT switching energy, ( E_{\text{total}} )</td>
<td>( f(x) = p_7 x^2 + p_8 x + p_9 )</td>
<td>( p_7 = 1.20 \times 10^{-7} ) ( p_8 = 2.14 \times 10^{-4} ) ( p_9 = 0.35 )</td>
<td>N/A</td>
<td>N/A</td>
<td>N/A</td>
</tr>
<tr>
<td>Diode recovery energy, ( E_{\text{rec}} )</td>
<td>( f(x) = p_{10} x^2 + p_{11} x + p_{12} )</td>
<td>( p_{10} = -4.04 \times 10^{-8} ) ( p_{11} = 3.92 \times 10^{-4} ) ( p_{12} = 0.2 )</td>
<td>0.00613</td>
<td>0.99</td>
<td>0.03</td>
</tr>
</tbody>
</table>

Fig. I-1. IGBT (a) turn-off and (b) turn-on energy of the devices used in the VSC model
The average energy loss over a switching cycle for the IGBT and diodes is calculated using (I.2) and (I.3) respectively.

\[
E_{IGBT\_average} = \frac{p7}{4}I_c^2 + \frac{p8}{\pi}I_c + \frac{p9}{2} \quad (I.2)
\]

\[
E_{rec\_average} = \frac{p10}{4}I_c^2 + \frac{p11}{\pi}I_c + \frac{p12}{2} \quad (I.3)
\]
Appendix J. Transformer efficiency

This appendix describes the method used to derive the transformer efficiency when considering the core and copper losses. The parameters of the transformer are provided in Table J-1, the per phase equivalent circuit is in Fig. 4-38 in chapter 4.

### Table J-1. Transformer parameters

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
<th>Unit</th>
</tr>
</thead>
<tbody>
<tr>
<td>Transformer rating, $P_n$</td>
<td>3</td>
<td>MW</td>
</tr>
<tr>
<td>Transformer primary voltage/secondary voltage $V_1/V_2$</td>
<td>480/690</td>
<td>V</td>
</tr>
<tr>
<td>Primary winding resistance, $R_1$</td>
<td>0.002</td>
<td>pu</td>
</tr>
<tr>
<td>Primary winding inductance, $L_1$</td>
<td>0.05</td>
<td>pu</td>
</tr>
<tr>
<td>Secondary winding resistance, $L_2$</td>
<td>0.002</td>
<td>pu</td>
</tr>
<tr>
<td>Secondary winding inductance, $L_2$</td>
<td>0.05</td>
<td>pu</td>
</tr>
<tr>
<td>Magnetization resistance, $R_m$</td>
<td>500</td>
<td>pu</td>
</tr>
<tr>
<td>Magnetization inductance, $L_m$</td>
<td>500</td>
<td>pu</td>
</tr>
</tbody>
</table>

The rated current at the output of the transformer is:

$$I_{rated} = \frac{P_n}{\sqrt{3}V_1 \cos \phi} = \frac{3 \times 10^6}{\sqrt{3} \times 480 \times 0.8} = 4500 \, A \tag{J.1}$$

The base values to calculate the actual resistance and inductance values are

$$S_{base} = \sqrt{3}V_1I_{rated} = 3.75 \, MVA \tag{J.2}$$

$$Z_{base} = \frac{V^2}{S_{base}} = 0.089 \, \Omega \tag{J.3}$$

$$L_{base} = \frac{Z_{base}}{2\pi f} = 0.235 \, mH \tag{J.4}$$

The turns ratio, $a$ of the transformer is:

$$a = \frac{V_1}{V_2} = \frac{480}{690} = 0.7 \tag{J.5}$$
The actual resistance and inductance of primary side windings are

\[ R_{1\text{actual}} = Z_{\text{base}}R_1 = 0.12 \text{ m\Omega} \quad (J.6) \]

\[ L_{1\text{actual}} = L_{\text{base}}L_1 = 8.17 \text{ \mu H} \quad (J.7) \]

The resistance and inductance of the secondary side windings referred to the primary side are

\[ R_{2\text{actual}'} = Z_{\text{base}}R_2a^2 = 59.6 \text{ \mu \Omega} \quad (J.8) \]

\[ L_{2\text{actual}'} = L_{\text{base}}L_2a^2 = 3.95 \text{ \mu H} \quad (J.9) \]

The total winding resistance across the transformer is:

\[ R_{eq} = R_{1\text{actual}} + R_{2\text{actual}'} \quad (J.10) \]

The copper losses in the transformer are therefore:

\[ P_{cu} = 3R_{eq}I_1^2 \quad (J.11) \]

The real core resistance referred to the primary side is:

\[ R_{\text{core}} = Z_{\text{base}}R_m \quad (J.12) \]

The voltage referred to the primary side to calculate the core losses are:

\[ V_{2'} = V_1 - I_1Z_1 \quad (J.13) \]

Where the total impedance across the primary side windings is

\[ Z_1 = 3[R_1 + (2\pi f L_1)] \quad (J.14) \]
The core loss can be estimated using (Sudhoff, 2014):

\[ P_{\text{core}} = \frac{V_2'^2}{R_{\text{core}}} \]  \hspace{1cm} (J.15)

The total losses are therefore:

\[ Losses = P_{\text{cu}} + P_{\text{core}} \]  \hspace{1cm} (J.16)

The efficiency of the transformer therefore is:

\[ \eta_{\text{tx}} = \frac{P_{\text{out}}}{P_{\text{out}} + Losses} \]  \hspace{1cm} (J.17)
Appendix K. Diesel generator schematics

This appendix contains the DG model schematic as built in the Simulink® modelling environment. This model is described in detail in chapter 4.

Fig. K-1 shows the DG schematic. The engine is shown in the top left of the schematic that is connected to the AVR exciter and machine block. The fuel consumption of the engine is calculated by the block in the top right which is described in section 4.9.2 of chapter 4, based on the mechanical power of the engine. The real and reactive power are fed back to the droop control layer of the power management architecture.

Fig. K-1. DG Simulink model schematic

Fig. K-2 shows the contents of the SimPowerSystems diesel engine block. The governor control system, governor actuator and engine time delay are shown. Mechanical power of the engine is calculated as a product of the engine speed, \( \omega \) and the engine torque, \( T \).

Fig. K-2. Simulink diesel engine block as provided in the SimPowerSystems block library

The SimPowerSystems Type 2 AVR and synchronous machine block are shown in Fig. K-3. The AVR
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block is shown in Fig. K-4. A full description of this model can be found in section 4.9.1. The droop control implementation is shown in Fig. K-5.

Fig. K-3. Simulink IEEE type 2 AVR and synchronous generator model schematic

Fig. K-4. Simulink IEEE Type 2 AVR model schematic

Fig. K-5. Droop controller schematic implemented in Simulink
Appendix L. DC/DC converter and control system model schematics

This appendix contains the modelling tool 3 schematic as built in the Simulink® modelling environment. This model is described in detail in Chapter 4.

Fig. L-1. Modelling tool 3 Simulink schematic

Fig. L-1 shows the top level schematic of the modelling tool used in the LDEW study. A full description of the constituent parts of this model can be found in Chapter 4. Shown in Fig. L-1 is the model of the battery ESS, DC/DC boost converter, control system and the LDEW load.

Fig. L-2. DC/DC converter control system implemented in Simulink

Fig. L-2 shows the DC/DC converter controller schematic. A full description of the controller can be found in section 4.12.
Fig. L-3 shows the variable resistor schematic used to emulate the LDEW load, the variable resistor value is looked up from the MATLAB workspace, to generate the pulse load profile discussed in Chapter 3.
Appendix M. **AVR verification**

This appendix contains the DG verification results that support the verification tests described in section 4.13.2. Fig. M-1 and Fig. M-2 show the verification response that support the test results for tests 5 through 8 in described in Table 4-21 in chapter 4.

Fig. M-1. (a) and (b) AVR test 5 results and (c) and (d) AVR test 6 results

Fig. M-2. (a) and (b) AVR test 7 results and (c) and (d) AVR test 8 results
Appendix N. Generator model verification

This appendix describes the theory used to calculate the theoretical short circuit current response of the generator characteristics from (AvK-Alternators, 2014). Theoretical explanation is proceeded by plots of the theoretical and simulated response.

In a three-phase fault, only the positive sequence sub-network is active (Prousalidis, 2018), the equivalent circuit is shown below in Fig. N-1 where, $E^-$ is the sub transient emf, $R_a$ is armature resistance and $X_d^\prime$ is the sub transient reactance.

![Equivalent Circuit](image)

**Fig. N-1. Single-phase equivalent circuit of the positive sequence sub-network during a three-phase fault**

The short circuit response is calculated in accordance with the procedure defined in IEC 61363 for mobile and offshore platforms (IEC, 1998). Where the sub-transient and transient emfs are given by:

$$E_q^- = \sqrt{\left(\frac{U_g}{\sqrt{3}}\cos\phi + R_a I_n\right)^2 + \left(\frac{U_g}{\sqrt{3}}\sin\phi + X_d^\prime I_n\right)^2} \quad (N.1)$$

$$E_q^\prime = \sqrt{\left(\frac{U_g}{\sqrt{3}}\cos\phi + R_a I_n\right)^2 + \left(\frac{U_g}{\sqrt{3}}\sin\phi + X_d^\prime I_n\right)^2} \quad (N.2)$$

where $U_g$ is the rated voltage of the machine at 690 V, $\phi$ is the power factor, set at 1. $I_n$ is the rated current of the machine, 2937 A and $X_d^\prime$ is the transient reactance. The AC component of the short circuit current, $I_{ac}(t)$ is characterised by:

$$I_{ac}(t) = (I_{kd} - I_{kd}^\prime) e^{-\frac{t}{\tau_d}} + (I_{kd}' - I_{kd}) e^{-\frac{t}{\tau_d}} + I_{kd} \quad (N.3)$$

where $I_{kd}^\prime$, $I_{kd}'$ and $I_{kd}$ are the sub-transient, transient, and steady state fault currents. These are calculated using:

$$I_{kd}^\prime = \frac{E_q^-}{\sqrt{R_a^2 + X_d^\prime}} \quad (N.4)$$
The DC component, $I_{DC}$, is given by

$$I_{DC}(t) = \sqrt{2}(I'_{kd} - \sin \phi) e^{-\frac{t}{\tau_{DC}}}$$  \hspace{1cm} (N.7)

The expression for the short-circuit waveform is the combination of the dc and ac components:

$$i_k(t) = \sqrt{2} I_{AC}(t) + I_{DC}(t)$$  \hspace{1cm} (N.8)

The sinusoidal expression for the short circuit waveform is given as:

$$i_k(t) = \sqrt{2} \left[ (I'_{kd} - I'_{kd}) e^{-\frac{t}{\tau_{a}}} \sin(\omega t - \alpha) + (I'_d - I_{kd}) e^{-\frac{t}{\tau_{a}}} \sin(\omega t - \alpha) \ight. \]

$$+ \left. I_{kd} \sin(\omega t + \alpha) + I_{kd} e^{-\frac{t}{\tau_{DC}}} \sin \alpha \right]$$  \hspace{1cm} (N.9)

The three-phase short circuit current characteristic waveform of the generator using the generator data described in Chapter 4 has been used to plot the characteristic components in Fig. N-2, showing the sub-transient phase (initial 4-5 cycles), transient, steady state and DC components. These are combined in Fig. N-3, with the upper and lower envelope of the faulted current.
Fig. N-2. Synchronous generator theoretical phase A (a) sub-transient (b) transient (c) steady state and (d) dc component response to short circuit

Fig. N-3. Combined theoretical components of synchronous generator phase A current response to short circuit showing impulse short circuit current
Appendix O. Synchroniser model schematic

This appendix contains the synchroniser model schematic as built in the Simulink® modelling environment that synchronises the ESS output with the main bus AC voltage. This model is described in detail in Chapter 4. Fig. O-1 shows the synchroniser top level schematic, the readings from the three phase grid and ESS output are fed to the synchroniser, the output offsets for voltage and frequency are routed to the VSC control in the secondary control layer. The third output is the breaker signal that follows the logic described in section 4.11.1.

![Simulink model of synchroniser](image)

**Fig. O-1.** Synchroniser top level model in Simulink

The breaker logic is shown in more detail below in Fig. O-2, this is detailed in section 4.11.1.

![Breaker logic](image)

**Fig. O-2.** Synchroniser breaker logic implemented in Simulink for voltage amplitude, phase and frequency

Fig. O-3 shows the grid synchroniser circuit in detail. The annotations to the Simulink schematic in Fig. O-3 show the key parts of the circuit including PLLs, delay for initialisation, PI controllers, offset and breaker signals.
Fig. O-3. Synchroniser control system schematic in Simulink
Appendix P.  Emissions factor method and supporting data

This appendix contains the supporting evidence of the emissions factor method followed to generate the GHG emission results for the operating profiles of the candidate warship in chapter 5. Table P-1 details the emission factor equations followed and reference sources to generate the emissions factors presented in Table P-2.

Table P-1. Equations used to calculate emission consumption in g/kWh

<table>
<thead>
<tr>
<th>Emission factor (EF)</th>
<th>g/kWh of Emissions</th>
<th>References</th>
</tr>
</thead>
<tbody>
<tr>
<td>Carbon Dioxide (CO₂)</td>
<td>3.206 ( (g \text{ CO}_2/g \text{ fuel}) \times SFC )</td>
<td>For MDO from Annex 8 of MEPC 63/21 (IMO, 2012)</td>
</tr>
<tr>
<td>Nitrogen Oxides (NOₓ)</td>
<td>9 n^{-0.2}, where n is the diesel engine speed, assumed as 1800 rpm.</td>
<td>Assuming IMO Tier III compliant high speed diesel engine (IMO, 2019b)</td>
</tr>
<tr>
<td>Sulphur Oxides (SO₂)</td>
<td>( SFC \times 2 \times 0.97753 \times \text{ sulphur FCF} )</td>
<td></td>
</tr>
<tr>
<td>Particulate Matter (PM)</td>
<td>( 0.23 + [SFC \times 7 \times 0.0247(\text{sulphur FCF} - 0.0024) )</td>
<td></td>
</tr>
<tr>
<td>Carbon Monoxide (CO)</td>
<td>-</td>
<td></td>
</tr>
<tr>
<td>Methane (CH₄)</td>
<td>0.02 \times (\text{NMVOC EF})</td>
<td>Table 22, Annex 6 of the Third IMO GHG study (IMO, 2015)</td>
</tr>
<tr>
<td>Nitrous Oxide (N₂O)</td>
<td>0.16 \times (SFC/1000)</td>
<td></td>
</tr>
<tr>
<td>Non-methane volatile organic compounds (NMVOC)</td>
<td>-</td>
<td></td>
</tr>
</tbody>
</table>

Table P-2. DG EFs at 80% MCR for MGO

<table>
<thead>
<tr>
<th>Emission factor (EF) of MGO for high speed diesel engine</th>
<th>SFC (g/kWh)</th>
<th>(g/kWh)</th>
<th>FCF</th>
<th>g pollutant/g fuel</th>
<th>DG (kg/tonne of fuel)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Carbon Dioxide (CO₂)</td>
<td>214</td>
<td>686.084</td>
<td>1.00</td>
<td>3.20600</td>
<td>3206.00</td>
</tr>
<tr>
<td>Nitrogen Oxides (NOₓ)</td>
<td>214</td>
<td>2.010</td>
<td>1.00</td>
<td>0.00939</td>
<td>9.39</td>
</tr>
<tr>
<td>Sulphur Oxides (SO₂)</td>
<td>214</td>
<td>41.838</td>
<td>0.05</td>
<td>0.00978</td>
<td>9.78</td>
</tr>
<tr>
<td>Particulate Matter (PM)</td>
<td>214</td>
<td>3.515</td>
<td>0.14</td>
<td>0.00230</td>
<td>2.30</td>
</tr>
<tr>
<td>Carbon Monoxide (CO)</td>
<td>214</td>
<td>0.540</td>
<td>1.00</td>
<td>0.00252</td>
<td>2.52</td>
</tr>
<tr>
<td>Methane (CH₄)</td>
<td>214</td>
<td>0.008</td>
<td>1.00</td>
<td>0.00004</td>
<td>0.04</td>
</tr>
<tr>
<td>Nitrous Oxide (N₂O)</td>
<td>214</td>
<td>0.034</td>
<td>1.00</td>
<td>0.00016</td>
<td>0.16</td>
</tr>
<tr>
<td>Non-methane volatile organic compounds (NMVOC)</td>
<td>214</td>
<td>0.400</td>
<td>1.00</td>
<td>0.00187</td>
<td>1.87</td>
</tr>
</tbody>
</table>
Appendix Q.  Power reserve results for temperate and tropical conditions

This appendix contains the power reserve results for the temperate and tropical condition operating profiles that support the results presented in Chapter 5.

Winter condition results

Fig. Q-1 through Q-3 present the total fuel burnt, DG running hours and exhaust GHG emissions during winter conditions. These results support the power reserve case study in Chapter 5.

Fig. Q-1 Total fuel burnt with and without ESS for each operating profile under winter conditions

Fig. Q-2. DG running hours with and without ESS for each operating profile under winter conditions
Fig. Q-3. DG GHG emissions over the operating profile with and without ESS for each operating profile under winter conditions.

Tropical climate condition results

Fig. Q-4 through Fig. Q-8 present the tropical climate condition results with and without the battery ESS integrated with the candidate warship electric power system.

Fig. Q-4. DG and GT MCR of the candidate ship during each operating state under tropical conditions with and without ESS.
Fig. Q-5. DG fuel burn during each operating state with and without ESS under tropical conditions

Fig. Q-6. Total fuel burnt over the operating profile with and without ESS under tropical conditions

Fig. Q-7. DG running hours with and without ESS for each operating profile under tropical conditions
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Fig. Q-8. DG GHG emissions over the operating profile with and without ESS for each operating profile under tropical conditions

Temperate results

Fig. Q-9 through Fig. Q-13 present the tropical climate condition results with and without the battery ESS integrated with the candidate warship electric power system.

Fig. Q-9. DG and GT MCR of the candidate ship during each operating state under temperate conditions with and without ESS
Fig. Q-10. DG fuel burn during each operating state with and without ESS under tropical conditions

Fig. Q-11. Total fuel burnt over the operating profile with and without ESS under temperate conditions

Fig. Q-12. DG running hours with and without ESS for each operating profile under temperate conditions
Fig. Q-13. DG GHG emissions over the operating profile with and without ESS for each operating profile under temperate conditions.
Appendix R. **Parallel DG results to load profiles 1-5**

This appendix contains the simulation results of the two parallel DGs when subjected to load profiles 1-5 described in section 5.4. The results are presented in Fig. R-1 through R-5 for the respective load profiles. Note DG1 and DG2 equally share the real and reactive power demand of the load, thus the power output of DG1 and DG2 are equal.

![Fig. R-1. Parallel DG (a) real power (b) reactive power (c) system frequency and (d) system voltage response to load profile 1](image-url)
Fig. R-2. Parallel DG (a) real power (b) reactive power (c) system frequency and (d) system voltage response to load profile 2

Fig. R-3. Parallel DG (a) real power (b) reactive power (c) system frequency and (d) system voltage response to load profile 3
Fig. R-4. Parallel DG (a) real power (b) reactive power (c) system frequency and (d) system voltage response to load profile 4

Fig. R-5. Parallel DG (a) real power (b) reactive power (c) system frequency and (d) system voltage response to load profile 5
Appendix S.  Generator capability plots

This appendix contains the generator capability plot results for load profiles 2-5 used in the load levelling investigation in chapter 5. Discussion of the load profile 1 response is provided in section 5.5. Fig. S-1 presents the DG response for during load profile 2. When the ESS is not load levelling, the power factor is constant at 0.85, when the ESS is load levelling, the reactive power and therefore reactive power is variable as shown by point A in Fig. S-1 between 0.95 and 0.75 PF lag, the reasons for which are discussed in section 5.5. This response is also exhibited for load profiles 3 through 5 in Fig. S-2 to S-4 respectively.

Fig. S-1. Generator capability plot under load profile 2

Fig. S-2. Generator capability plot under load profile 3
Fig. S-3. Generator capability plot under load profile 4

Fig. S-4. Generator capability plot under load profile 5
Appendix T. Summary of fuel and GHG emissions results for load levelling investigation

This appendix details the supporting data for the results for the five load levelling profiles presented in the load levelling investigation in chapter 5. Table T-1 details the parallel DG operation fuel and exhaust emissions results. Table T-2 summarises the ESS results.

Table T-3 and Table T-4 present the diesel engine fuel performance when the ESS is in load levelling mode and power reserve mode respectively.

Table T-1. Parallel DG operation simulation results

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<th>Profile</th>
<th>Mass fuel (kg)</th>
<th>Fuel energy (MJ)</th>
<th>CO2 (kg)</th>
<th>NoX (kg)</th>
<th>SoX (kg)</th>
<th>PM (kg)</th>
<th>Co (kg)</th>
<th>Ch4 (kg)</th>
<th>N2O (kg)</th>
<th>NMVOC (kg)</th>
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Table T-2. ESS simulation results under load levelling load profiles

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<th>Battery energy used (kWh)</th>
<th>Battery energy (MJ)</th>
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Table T-3. Diesel engine simulation results when the ESS is in load levelling mode

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<th>SoX (kg)</th>
<th>PM (kg)</th>
<th>Co (kg)</th>
<th>Ch4 (kg)</th>
<th>N2O (kg)</th>
<th>NMVOC (kg)</th>
<th>Total emissions (kg)</th>
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Table T-4. Power reserve mode simulation results

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<th>NoX (kg)</th>
<th>SoX (kg)</th>
<th>PM (kg)</th>
<th>Co (kg)</th>
<th>Ch4 (kg)</th>
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Appendix U. Supporting results for battery voltage and current response to LDEW load

This appendix presents the result obtained that support the discussion presented in section 6.3.3. For comparison the battery voltage response is shown in Fig. U-1 and the current response is shown in Fig. U-2 for when $T_r$ is 50 ms and 100 ms.

![Battery voltage response to LDEW loading](image)

**Fig. U-1.** Battery voltage response to LDEW loading when the simulation commenced at (a) 90% and (b) 70% initial SoC when $T_r = 50$ ms, equivalently for (c) and (d), when $T_r = 100$ ms.
Appendices

![Battery current response to LDEW loading](image)

Fig. U-2. Battery current response to LDEW loading when the simulation commenced at (a) 90% and (b) 70% initial SoC when $T_r = 50$ ms, equivalently for (c) and (d), when $T_r = 100$ ms.
Appendix V. Supporting results for ESS and converter losses

This appendix contains supporting evidence for the results presented in section 6.3. The energy supplied by the ESS and delivered to the LDEW load for the 1.75 MW and 1.5 MW case are presented in, Fig. V-1 and Fig. V-2.

Fig. V-1. (a) Energy supplied by the ESS and delivered to the load, (b) load power and (c) ESS efficiency during 1.75 MW, four-minute engagement scenario
Appendices

Fig. V-2. (a) Energy supplied by the ESS and delivered to the load, (b) load power and (c) ESS efficiency during 1.5 MW, four-minute engagement scenario

Fig. V-3 through Fig. V-5 presents the supporting plots for Table 6-6 for the 2 MW, 1.75 MW and 1.5 MW LDEW loading condition respectively.

Fig. V-3. (a) ESS efficiency (b) ESS losses (c) Total losses including ESS and DC/DC converter and (d) ESS exothermic loss over 2 MW LDEW four minute engagement
Appendices

Fig. V-4. (a) ESS efficiency (b) ESS losses (c) Total losses including ESS and DC/DC converter and (d) ESS exothermic loss over 1.75 MW LDEW four minute engagement

Fig. V-5. (a) ESS efficiency (b) ESS losses (c) Total losses including ESS and DC/DC converter and (d) ESS exothermic loss over 1.5 MW LDEW four minute engagement
Appendix W. Supporting results for recharging study

This appendix contains the recharge time results for the 2 MW, 1.75 MW and 1.5 MW LDEW loading conditions in Table W-1, Table W-2, and Table W-3 respectively. The presented results provide supporting evidence for the discussion in section 6.3.7.

Table W-1. Recharge results following a four minute 2 MW LDEW loading condition

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<tr>
<th>Initial SoC (%)</th>
<th>Energy at initial SoC (MJ)</th>
<th>Energy supplied by ESS (MJ)</th>
<th>Energy remaining in ESS after 4 minute LDEW operation (MJ)</th>
<th>SoC after 4 minute LDEW operation (%)</th>
<th>Simulated recharge time to 83% SoC (s)</th>
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Table W-2. Recharge results following a four minute 1.75 MW LDEW loading condition

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<th>Energy at initial SoC (MJ)</th>
<th>Energy supplied by ESS (MJ)</th>
<th>Energy remaining in ESS after 4 minute LDEW operation (MJ)</th>
<th>SoC after 4 minute LDEW operation (%)</th>
<th>Simulated recharge time to 66.7% SoC (s)</th>
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Table W-3. Recharge results following a four minute 1.5 MW LDEW loading condition

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<th>Energy at initial SoC (MJ)</th>
<th>Energy supplied by ESS (MJ)</th>
<th>Energy remaining in ESS after 4 minute LDEW operation (MJ)</th>
<th>SoC after 4 minute LDEW operation (%)</th>
<th>Simulated recharge time to 45% SoC (s)</th>
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Appendix X. Full degradation study results

This appendix presents the full results of the degraded performance of the battery ESS described in section 6.4. Table X-1 details the simulation results for the minimum initial SoC case under degraded conditions for each LDEW power investigated in chapter 6.

Table X-2 details the simulation results when the ESS commences a four minute LDEW engagement at 90% SoC for BoL and degraded conditions 1 through 5.

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<th>Minimum SoC to meet 4 minute requirement (%)</th>
<th>Bus V max (V)</th>
<th>Deviation (%)</th>
<th>Bus V Min (V)</th>
<th>Deviation (%)</th>
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Table X-2. Spreadsheet recorded results of degraded performance for ESS 90% initial SoC condition and $T_r=25$ ms

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