Fatigue and Fracture of Tubulars Containing Large Cracks

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ABSTRACT

This thesis presents an investigation into the performance of offshore tubular components containing large defects. The significance of the residual strength of cracked tubular members is considered with respect to inspection and maintenance of structural integrity.

A series of nine destructive static strength tests were performed on full-scale pre-cracked tubular welded T and Y-joints manufactured from a weldable high strength steel (Superelso 702), which is utilised in the construction of offshore Jack-Up platforms. All specimens had at least one through-thickness fatigue crack at the weld toe, from a previous fatigue-testing programme.

Static strength tests on four large tubular sections manufactured from BS7191 355D were also carried out. The specimens contained either a through-thickness or a part-through-thickness defect.

A novel digital photogrammetry technique was utilised to maximise the data collection from the destructive tests. The method is capable of the quantification of three-dimensional displacements, which subsequently allowed for a better understanding of the behaviour of the specimens during the tests.

A fracture mechanics study of tubular components containing large cracks is presented. The limited number of stress intensity factor (SIF) solutions for cracks in tubular sections are considered and a new SIF solution for tubular T-joints, containing through-thickness cracks, under axial loading is provided. The method is based on the SIF at the crack tip and the non-uniform stress distribution present in an axially loaded tubular T-joint. The information has been integrated into the safety evaluation of all specimens using a failure assessment diagram (FAD) procedure.

Finally, the local and the global responses of a structure to the presence of a large defect are reviewed. The importance of redundancy and multiple load paths are stressed and possible repair and maintenance options are considered.
This thesis is dedicated to the acknowledged.
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NOMENCLATURE

\(a\)  \hspace{1cm} \text{Crack depth}
\(a_i\)  \hspace{1cm} \text{Initial crack depth}
\(a_f\)  \hspace{1cm} \text{Final crack depth}
\(A\)  \hspace{1cm} \text{Pipe geometry dependent variable}
\(A_c\)  \hspace{1cm} \text{Crack area}
\(A_{bm}, A_{bb}\)  \hspace{1cm} \text{Constants for the API 579 SIF equations under bending}
\(A_{mn}, A_{mb}\)  \hspace{1cm} \text{Constants for the API 579 SIF equations under tension}
\(ACFM\)  \hspace{1cm} \text{Alternating Current Field Measurement}
\(ACPD\)  \hspace{1cm} \text{Alternating Current Potential Drop}
\(ACSM\)  \hspace{1cm} \text{Alternating Current Stress Measurement}
\(API\)  \hspace{1cm} \text{American Petroleum Institute}
\(b\)  \hspace{1cm} \text{Half cylinder circumference or half plate width}
\(B\)  \hspace{1cm} \text{Section thickness in plane of flaw}
\(c\)  \hspace{1cm} \text{Half crack length}
\(C\)  \hspace{1cm} \text{Paris constant}
\(CCT\)  \hspace{1cm} \text{Centre-Cracked Tension}
\(COD\)  \hspace{1cm} \text{Crack Opening Displacement}
\(CP\)  \hspace{1cm} \text{Cathodic Protection}
\(CTOD\)  \hspace{1cm} \text{Crack Tip Opening Displacement}
\(CVI\)  \hspace{1cm} \text{Close Visual Inspection}
\(d\)  \hspace{1cm} \text{Brace diameter}
\(da/dN\)  \hspace{1cm} \text{Crack growth rate}
\(D\)  \hspace{1cm} \text{Chord diameter}
\(DoB\)  \hspace{1cm} \text{Degree of Bending}
\(E\)  \hspace{1cm} \text{Young's Modulus}
\(ECM\)  \hspace{1cm} \text{Electrochemical Machining}
\(EPFM\)  \hspace{1cm} \text{Elastic-Plastic Fracture Mechanics}
\(F_{AR}\)  \hspace{1cm} \text{Area reduction factor}
\( F_b \) Normalised SIF under bending (Zahoor)  
\( F_t \) Normalised SIF under tension (Zahoor)  
\( F_o \) Dimensionless SIF under tension (Sanders)  
\( F_y \) Characteristic yield strength of the chord member or  
\( 0.7 \) times the characteristic tensile strength, if less (HSE)  
\( F_{ye} \) Yield strength of the chord member or \( \frac{2}{3} \) of the tensile  
strength, if less (API RP 2A)  
\( FAD \) Failure Assessment Diagram  
\( FMD \) Flooded Member Detection  
\( G_b \) Stress intensity components under bending (Kumosa  
and Hull)  
\( G_m \) Stress intensity components under tension (Kumosa  
and Hull)  
\( GVI \) General Visual Inspection  
\( h \) Half plate length  
\( HAZ \) Heat Affected Zone  
\( I \) Dimensionless energy release rate  
\( IPB \) In-plane bending  
\( IRM \) Inspection Repair Maintenance  
\( ISO \) International Organization for Standardization  
\( J \) J-integral  
\( K \) Stress intensity factor  
\( K \) S-N curve intercept  
\( K_l \) Mode I SIF  
\( K_{lc} \) Mode I fracture toughness  
\( K_c \) Critical SIF  
\( K_r \) Stress intensity ratio  
\( \Delta K_{th} \) Threshold SIF  
\( l \) Brace length  
\( l_w \) Intersection weld length  
\( L \) Chord length
\( L_r \)  
Load ratio

\( L_{r_{\text{max}}} \)  
Maximum load ratio

LEFM  
Linear Elastic Fracture Mechanics

LRFD  
Load and Resistance Factor Design

LVDT  
Linear Variable Displacement Transducer

\( m \)  
Gradient of S-N curve

\( m \)  
Paris constant

\( m_q \)  
Power coefficient used in API RP 2A and HSE design calculations

\( M_a \)  
Allowable capacity for brace bending moment (API RP 2A)

\( M_{si} \)  
Applied in-plane bending moment (BS 7910)

\( M_{so} \)  
Applied out-of-plane bending moment (BS 7910)

\( M_{ci} \)  
Yield strength based collapse loads for in-plane bending moment \( \times F_{AR} \) (BS 7910)

\( M_{co} \)  
Yield strength based collapse loads for out-of-plane bending moment \( \times F_{AR} \) (BS 7910)

\( M_{ki} \)  
Characteristic strength for brace in-plane moment load (HSE)

\( M_{ko} \)  
Characteristic strength for brace out-of-plane moment load (HSE)

\( M_b \)  
Normalised SIF under bending (API 579)

\( M_m \)  
Normalised SIF under tension (API 579)

MMA  
Manual Metal Arc

MPI  
Magnetic Particle Inspection

\( N \)  
Number of cycles

NDT  
Non-Destructive Testing

OPB  
Out-of-plane bending

\( P \)  
Applied load

\( P_a \)  
Applied axial load (BS 7910)
P_c Yield strength based collapse load for axial loading \times F_{AR} (BS 7910)

P_a Allowable capacity for brace axial load (API RP 2A)

P_k Characteristic strength for brace axial load (HSE)

P_u Ultimate capacity (Pan et al.)

POD Probability of Detection

PWHT Post Weld Heat Treatment

Q_f Nominal longitudinal stress factor (API RP 2A)

Q_u Ultimate strength factor (API RP 2A)

Q_f Factor to allow for the presence of axial and moment loads in the chord (HSE)

Q_u Strength factor (HSE)

Q_\beta Coefficient accounting for the increased strength at high \beta ratios

r Distance from the crack tip

R Mean radius

R_i Inner radius

R_o Outer radius

RIF Residual Resistance Factor

ROV Remotely Operated Vehicle

RSR Reserve Strength Ratio

\Delta S Hot spot stress range

S_r Stress ratio

SCF Stress Concentration Factor

SCXI Signal Conditioning Extensions for Installations

SIF Stress Intensity Factor

SSRT Slow Strain Rate Test

t Brace thickness

t_{rem} Remaining thickness

T Chord thickness

TIG Tungsten Inert Gas
WSD  Working Stress Design

xi  Increments along the crack path

Y  Modification factor

Y_{exp}  Modification factor derived from experimental data

Y_g  Non-uniform stress correction factor

Y_T  Modification factor for axially loaded T-joints

a  Non-dimensional joint geometry factor, 2L/D

a  Half crack angle (Kumosa and Hull)

β  Non-dimensional joint geometry factor, d/D

γ  Non-dimensional joint geometry factor, D/2T

δ  CTOD

δ_{crit}  Critical CTOD

δ_{i}  Applied CTOD

δ_{lc}  Mode I critical CTOD

δ_f  Fracture ratio using CTOD parameter

ε_f  Failure strain

ε_y  Elastic yield strain

θ  Half crack angle (Zahoor)

θ  Brace angle

ν  Poisson’s ratio

σ  Nominal stress

σ^\infty  Uniform remote tensile stress

σ_b  Bending stress in a pipe (Zahoor)

σ_{flow}  Flow stress

σ_m  Nominal stress (Kumosa and Hull)

σ_{max}  Maximum applied stress

σ_{net}  Net section stress

σ_{ref}  Reference stress

σ_t  Membrane stress in a pipe (Zahoor)

σ_{uts}  Ultimate tensile stress
<table>
<thead>
<tr>
<th>Symbol</th>
<th>Description</th>
</tr>
</thead>
<tbody>
<tr>
<td>$\sigma_x$</td>
<td>Stress at a particular point around the intersection</td>
</tr>
<tr>
<td>$\sigma_{xi}$</td>
<td>Stress at a particular increment along the crack path</td>
</tr>
<tr>
<td>$\sigma_y$</td>
<td>Yield strength</td>
</tr>
<tr>
<td>$\sigma_{yy}$</td>
<td>Stress normal to the crack plane</td>
</tr>
<tr>
<td>$\sigma_B$</td>
<td>Bending stress</td>
</tr>
<tr>
<td>$\sigma_M$</td>
<td>Membrane stress</td>
</tr>
<tr>
<td>$\sigma_T$</td>
<td>Total stress</td>
</tr>
<tr>
<td>$\tau$</td>
<td>Non-dimensional joint geometry factor, $t/T$</td>
</tr>
</tbody>
</table>
CHAPTER 1

1.0 INTRODUCTION AND BACKGROUND

1.1 INTRODUCTION

Tubular joints provide offshore steel structures with their primary resistance to environmental loading. The design and construction of these joints and their in-service inspection and maintenance are major items in the cost of offshore platforms. During its design life, an offshore structure is subjected to extreme static and fatigue loading due to severe environmental conditions, eventually leading to initiation of defects such as cracks and corrosion pits. In order to ensure structural integrity, regular inspection programmes are required.

Inspection scheduling for various sections of the structure will depend on the design methodology used in the construction of that section. For non-primary nodes, current practices rely on techniques which detect flooding of members resulting from through-thickness cracking, such as flooded member detection and visual techniques, in preference to other techniques employed for in-service inspection of fixed platforms. This indicates that some defects are not discovered until after extensive propagation in the joints, thus reducing both, the member and the overall platform residual strength.

Inspection capability declines as defect size decreases. This could mean that small defects could be missed or undersized, particularly if the near threshold level of the inspection technique is in use. Without the presence of adequate load redistribution within the structure, these defects may develop to large and possibly through-thickness cracks by the next scheduled inspection and as such might greatly reduce the strength of the tubular joint.
If a through-thickness crack has formed, the remaining fatigue life is diminished, the static strength of the cracked joint might be significantly reduced compared to that of an uncracked joint, and repair options become limited. It is therefore important to consider the capacity of welded components containing through-thickness cracks as this would help to better evaluate the situation and could possibly save unnecessary costs of total member replacement.

1.2 DESIGN METHODOLOGIES

There are several accepted approaches to the design of conventional and offshore structures. It is up to the designer to decide which is the most appropriate. Two of the factors that will govern the decision are the degree of safety required, and inspection, repair and maintenance of the possible defect. The most commonly practised philosophies are safe-life, fail-safe, and defect tolerant approaches [1.1, 1.2].

The safe-life design concept is aimed at ensuring that a particular component, or a complete structure, will not fail in service within its designed life, which may be infinite. When the design life is reached, the part concerned is discarded. The concept is usually applied to structural components which experience fatigue loading, whose failure may induce catastrophic failure of the structure. This means that the designer must know the loading condition during the life of the structure, and the effect of such loading on the material, to enable the prediction of the service life in terms of number of cycles to failure. The philosophy normally requires the use of large factors of safety to allow for a number of unknowns. In particular, it has to cover the normal scatter obtained in fatigue test results, the errors in estimations of the load spectrum, and the effect of other variables such as corrosion.

The fail-safe method assumes that in spite of failure of an individual component, there will always be sufficient strength and stiffness in the remaining part of the structure to be used safely until the failure is discovered and subsequent repairs are implemented.
The concept requires the knowledge of critical areas and failure modes from full-scale testing. *Fail-safe* design may be achieved by incorporating crack arresters at various locations within the structure and by the addition of multiple load paths to provide a higher degree of redundancy. In addition, the structural elements must be arranged so as to make regular inspection as easy as possible. The *fail-safe* design philosophy need not be limited to structural members loaded in fatigue. Members in which brittle fracture is possible, or in which stress corrosion cracks may occur, could also be designed using a *fail-safe* concept.

Offshore structures have generally been designed using a combination of the *safe-life* and *fail-safe* design philosophies. The fatigue assessment of a *safe-life* concept becomes less critical with the merger of *fail-safe* features.

*Defect tolerant* design, or a philosophy of "living with cracks", requires that fatigue assessment be carried out using the assumption that cracks of predetermined minimum sizes are present at all critical locations, such as the weld toes. Successful implementation calls for setting explicit inspection intervals using appropriate techniques. The crack growth rate for particular geometries and environmental conditions is then determined. This is usually followed by methods to hinder the rate of further crack propagation.

Recently developed design approaches include *design for maintenance* and *controlled failure design* [1.3].

*Design for maintenance* means that inspection and repair are considered at the initial design stage. For example, a new material may exhibit attractive structural properties, but may not lend itself well to conventional non-destructive testing methods. In addition, accessibility and ease of Non-Destructive Testing (NDT) deployment may have a large influence on the Probability Of Detection (POD) for inspection. By designing for maintenance, a designer should allow for damage localisation and ease of exchange of components.
The lack of design for maintenance suggests that a safe-life design philosophy must be employed, incorporating large factors of safety. This will lead to over designing and subsequent repercussions such as the increase in the overall weight of the structure and economical losses. A compromise must be reached in that design for maintenance must be employed at all possible locations. If there are any locations where this might compromise safety, then an over designing approach must be undertaken.

Controlled failure design originates from the acceptance that not all failures can be avoided. Statistically, irrespective of the quality of design and maintenance operations, some failures will inevitably occur. Controlled failure design is targeted at managing the manner in which a structure or component may fail. For example, a slow ductile failure is often preferred over a rapid brittle fracture. Fatigue lends itself well to the requirement of a slow progressive failure as long as the onset of failure is apparent and its progression can be monitored and understood. Structural redundancy, load redistribution and understanding of the mechanistic failure mode mechanisms can all assist in the retardation of the final failure condition.

1.3 FAILURE MODES

One of the fundamental requirements of any engineering structure is that it should not fail in service. Much of the skill of the structural engineer lies in recognising that there are several possible modes of failure and in guarding against them in the design stage.

A global failure mode is any distinct sequence of local component failures that cause the structure to fail. For example, in a simple frame, plastic hinges can form leading to combined types of plastic mechanisms. In typical offshore structures, there are a large number of possible failure modes. Two of the more common modes of failure are described below.


**Fracture**

Throughout its service life, an offshore platform is exposed to environmental loading which cause cyclic stress variations in its structural members resulting in possible fatigue crack growth. Wave loading is the main source of potential fatigue cracking. However, any other source of cyclic loading, such as wind forces and mechanical vibrations, could contribute to fatigue damage. The magnitudes of these loads are far below the static failure loads of the structural components.

Fatigue cracks will normally grow at large section changes and other points of high stress concentration such as holes and welds. In simple tubular joints, fatigue cracks usually initiate as a surface crack from the weld toe and propagate into the tubular wall. The continuous cracking leads to load shedding and an overall weakening of the tubular strength. This eventually results in gross separation of the brace from the chord. Fatigue is arguably the most common type of failure and will be discussed in depth in future sections.

Brittle fracture occurs when the elastic deformation in a member is carried to the extreme. The load applied is absorbed up to a point where there is little strain or deformation prior to the member separating into two or more pieces.

In a component containing a fatigue crack, the application of adequate loading results in a mixture of ductile tearing followed by brittle fracture. The crack tips act as points of high stress concentration and extend to a certain point. Thereafter, the energy is absorbed by the remaining intact section of the connection until catastrophic failure.

**Environmental Effects**

The most common mode of failure caused by the environment is the effect of corrosion. Corrosion is a costly chemical process that burdens the offshore industry. It can be defined as the reaction of a metallic material with its environment, leading to undesired deterioration of the material. This results in the component concerned to be rendered incapable of performing its intended function.
Under corrosive conditions (acidic, aqueous, environmental) fatigue cracks may be initiated at a much earlier stage during the service life of a structure. Cracks may also grow faster in a corrosive environment. The combined action of corrosion and fatigue will therefore be responsible for failure following a smaller number of stress cycles than would be expected in a non-corrosive environment. In ferrous alloys, a fatigue (endurance) limit is obtained under non-corrosive fatigue conditions such that, at stress levels below this limit, fatigue failure is not expected. Corrosive conditions remove this limit, as shown in Figure 1.1, allowing for the possible failure of ferrous alloys even at low stress levels.

The problem of corrosion can to some extent be avoided by incorporating methods such as various forms of coatings or cathodic protection (CP), which may include the use of sacrificial anodes or induced current. Great care must be taken regarding the potential value of cathodic protection applied as it has been previously proven that a non-suitable CP level could either under-protect or greatly reduce fatigue life by embrittlement due to hydrogen [1.4].

The effects of residual and applied stresses and corrosive environments in service are closely interrelated. The more highly stressed regions of a metal will become anodic and thus leading to the creation of corrosive cells due to differences in local stress levels. The combined effect of stress and corrosion can result in a type of failure known as stress corrosion cracking. Stress corrosion cracks are nucleated at pitting damage sites and develop under the action of local tensile stresses as highly branched intergranular network of fine cracks. At each crack tip, the combined actions of the tensile stress and specific ions in the corrosive media, cause continual crack propagation with little evidence of local deformation.

A more common, but perhaps less troublesome, environmental consequence is the effect of temperature on a structure. In case of an offshore platform, a designer often has to take into account the operating surrounding temperature. The selection of material with an appropriate ductile-brittle transition temperature will to some extent
prevent brittle fracture of components in arctic waters. Modern steels generally tend to have low enough ductile-brittle transition temperatures for this to be only a relatively minor concern. In contrast, as the surrounding sea temperature is raised, there is a greater possibility of marine growth on the members. This can be detrimental in that there is an overall gain of the platform weight. Also, the drag coefficients of the components are increased and thus the wave and wind loading effects are escalated.

1.4 STRESS ANALYSIS OF TUBULAR JOINTS

The stress analysis of tubular joints is an important step in the design stage of an offshore platform. The joints need to be designed to be able to withstand operational loading and severe environmental conditions. Any combination of three loading modes may be applied to a member. Figure 1.2 illustrates axial loading, in-plane and out-of-plane bending on a T-joint. The resulting stresses could lead to failure of the member. Fatigue failure may occur even though the magnitudes of the nominal stresses are well below the static strength of the joints. The complex geometry of tubular intersections can result in high stress amplifications, which can subsequently lead to failure.

Stress analysis also provides vital information on the distribution of critical stresses in each component of the structure. The stress distribution across the anticipated crack path and through the wall thickness is important in both stress-life and fracture mechanics based approaches employed at the design stage and during structural integrity assessment (Section 1.5). The stresses present in a tubular joint can be divided into three categories.

Nominal stresses arise from the structural response of the joint to the applied load. They depend entirely on the dimensions of the joint and the mode of loading.
The application of load to the brace of an axially loaded joint will result in the extension of the brace and the bending of the chord wall. This is usually referred to as chord ovalisation and will result in the presence of geometric stresses. Figure 1.3 illustrates the bending of the chord with the application of load to the brace.

Stresses arising from the geometric discontinuity in a member, i.e. the weld, are termed notch stresses. While the first two types of stresses can be quantified using standard, experimental stress/strain measuring techniques, the stresses at the weld intersection are calculated by extrapolation of the linear stresses in the vicinity of the weld toe. The presence of large non-linear stress gradients at the weld toe implies that the value for stress obtained is at best an accurate approximation. Although stress gradients will always exist, they may be reduced by more controlled welding, resulting in a smaller weld angle and an increase in the weld toe radius [1.5]. Figure 1.4 illustrates the weld toe terminology.

The most common form of experimental stress/strain measurement is by the use of electrical resistance strain gauges. These are normally bonded to the component, outside the notch region due to the rapid increase and variations in stress around the weld toe. The steep stress gradient close to the intersection, combined with the fact that a strain gauge averages out the distribution of strains over the area on which it is located, result in a requirement for strain gauges with small gauge lengths in order to achieve maximum accuracy.

1.4.1 Stress Concentration Factor for Tubular Joints

As mentioned in the above section, the stress measured in the vicinity of the weld toe can be strongly influenced by the high stress gradient. The nominal stresses and hot spot stresses are commonly represented in terms of the stress concentration factor, SCF, as shown below.

\[
SCF = \sigma_s / \sigma
\]  

(1.1)
where: \( \sigma_x \) = Stress at a particular point around the intersection  
\( \sigma \) = Nominal stress

There are various definitions for the SCF. These may result in a different value of SCF for nominally identical joints. This is an important point to consider when comparing results from different studies.

The definition of hot spot stress SCF employed by the UK Health and Safety Executive (HSE) has been adopted for this study [1.6]. The hot spot stress is defined as “the greatest value around the brace/chord intersection of the extrapolation to the weld toe of the geometric stress distribution near the weld toe”. This hot spot stress incorporates the effects of overall joint geometry, i.e. the relative size of the brace and chord, but omits the stress concentrating influence of the weld itself that results in local stress distribution. Figures 1.5a and 1.5b illustrate the definition of hot spot stress and the stress distributions in the brace and chord as used in the now withdrawn UK guidance notes [1.6].

1.4.2 Parametric Equations for SCF Predictions

The determination of the stresses around tubular joint intersections by analytical techniques has proven to be very difficult, due to the relative complexity of the geometrical configuration. Therefore, parametric equations have been produced by various parties, which provide the SCF in terms of the non-dimensional parameters of the tubular joints, as defined in Table 1.1. In practice, only Finite Element Analysis (FEA) and strain gauge acrylic and steel model techniques have been used to provide the input data for parametric equations. A selection of the most commonly used methods to approximate SCFs is presented below.

**Kuang Equations**

The Kuang equations for T/Y, K, and KT-joint configurations utilise a modified thin shell FE program specifically designed to analyse tubular connections. The joints were modelled by intersecting thin shell cylinders representing the mid-planes. In
obtaining these equations Kuang assumed fixed chord end conditions to provide the necessary torsional restraint. It is important to note that a fillet weld was not present while modelling the tubular connections. Also, the stresses were measured at the mid-plane of the member wall. This study presents one of the earlier sets of parametric equations for SCFs in tubular joints.

The FE models incorporated by Kuang can neither account for the complex three-dimensional stress state, nor the geometric discontinuity at the weld toe. Therefore, the stresses calculated using the Kuang FE models are considerably different from the UK HSE definition of hot spot stresses [1.7, 1.8].

**Wordsworth/Smedley Equations**

The formulae (Wordsworth/Smedley, 1978 and Wordsworth, 1981) were derived using acrylic model test results (T, Y, KT, and X-joints) following the UK HSE recommendations for the derivation of the hot spot stresses. Small-scale acrylic models provided a cost effective alternative to full-scale testing. Strain gauges were bonded at certain points along the intersection, which does not include a modelled weld, in order to measure the local stresses.

The set of equations only provides the SCF at the crown and saddle positions along the chordside. Also, through-thickness stress distributions are not analysed [1.9, 1.8].

**Efthymiou/Durkin Equations**

The Efthymiou/Durkin equations for T/Y and gap/overlapped K-joints were expanded (Efthymiou, 1988) to cover T, Y, X, K, and KT-joints. The PMBSHELL program was used employing three-dimensional shell elements which enabled a realistic weld fillet and tube thickness to be modelled. In all, 150 tests under various loading conditions were monitored.
The main shortcoming of this study is that although it was possible to obtain information on the through-thickness stress distributions from the model, no equations are provided. It should, however, be mentioned that this was the first widely used set of parametric equations to have been obtained using a solid model and as such represents a breakthrough in determining the SCFs of tubular joints by an analytical method [1.10, 1.8].

**Lloyd’s Register Equations**

A set of parametric equations based on a database of screened steel and acrylic models were developed by Lloyd’s Register for T/Y, X, K, and KT-joints. These equations were derived using a multi-variable least square curve fitting routine, minimising the percentage difference between the recorded SCF and predicted SCF values. Consideration was given to design factors, and chord ovalisation effects due to the short chord length.

Two arguments can be made against the use of the Lloyds equations. Firstly, the database used is compiled from various studies. A slight variance in the models could add discrepancies to the equations. Secondly, the equations provide SCFs at a maximum of two positions and do not supply stress distributions around the intersection or through the member thickness [1.11, 1.8].

**Hellier, Connolly, and Dover Equations**

Hellier, Connolly and Dover carried out a comprehensive FE analysis on the stresses in T and Y-joints involving more than 900 thin shell models from a single database. Wide ranges of geometries under the three main loading modes were considered.

As with the Kuang equations, the weld was not modelled and the intersection between the chord and the brace was modelled as the intersection of their mid-planes. The main aim for this study was to be used as a fracture mechanics tool and thus, along with the SCFs at the crown and saddle points, the programme includes the stress distribution around the weld intersection as well as the degree of bending (ratio of
bending stress to total stress). The degree of bending equations provide information about the through-thickness stress distribution which is used to predict the Stress Intensity Factor (SIF) at the deepest point (described in Section 1.5.2) and hence the fatigue life of a tubular joint [1.12, 1.8].

**Chang and Dover Equations**

Chang and Dover conducted thin shell FE analysis using the ABAQUS software [1.13] for 660 Y, T, X, and DT-joints subjected to different modes of loading. The models were similar to the Hellier, Connolly and Dover analysis in that there was no modelling of the weld and that the intersection of the mid-planes represented the brace to chord intersection. The study resulted in the production of a new set of parametric equations which can be used to predict the hot spot SCF and the degree of bending at all critical positions for X and DT-joints. The equations also provided full SCF distribution along the chord and brace toe for Y, T, K, and DT-joints [1.14, 1.15].

The methods described above were utilised in Chapter 4 to calculate the SCF for a T-joint subjected to axial loading.

### 1.4.3 Stress Distributions

As mentioned in the introductory part of this subsection, applying a fracture mechanics approach to crack growth in tubular joints requires knowledge of the stresses acting across the anticipated crack path. It should be noted that there are two paths of crack propagation. The crack tends to follow the weld toe around the intersection as well as through the section thickness.

**Through-Thickness Stress Distribution**

The stress distribution through the wall thickness is assumed to be a linear combination of membrane and bending stresses. Due to ovalisation, the stresses in the outside wall of the chord or brace will be different to those in the inside wall. This discrepancy may to a greater extent be observed closer to the weld intersection. A
simple method to account for and relate the bending and membrane stresses to a value of total stress is shown below [1.16].

\[
\frac{\sigma_B}{\sigma_T} = \frac{\sigma_B}{\sigma_B + \sigma_M}
\]  

(1.2)

where:
- \(\sigma_B\) = Bending stress
- \(\sigma_T\) = Total stress
- \(\sigma_M\) = Membrane stress

**Stress Distribution along the Intersection**

Cracks do not only grow in the hot spot stress region. In many cases, multiple cracks initiate and propagate to form a larger defect. This implies that in order to employ a fracture mechanics approach, knowledge of the local stress distribution around the intersection is required. The distribution is dependent on the forces transmitted by the bracing. Figure 1.6 illustrates the SCF around the intersection of a T-joint resulting from a normal force in traction (axial), and bending moments, in the plane defined by the chord and the bracing (IPB) and out of this plane (OPB).

**1.4.4 Stresses in Cracked Members**

The dynamic loading of a tubular steel jacket offshore structure results in the growth of defects in the most highly stressed tubular intersections. The initial flaw or weld defect may be a surface crack, which by fatigue propagation, usually extends around the intersection before penetrating the thickness. The remaining life of the joint will be determined by the growth of the through-thickness crack.

Most offshore jacket structures have a degree of redundancy. A damaged joint may be manifested as an area of reduced local stiffness, resulting in changing load paths within the structure, a change in boundary conditions, and possible load shedding away from the damaged areas. A decrease in the forces and moments in the cracked section could discontinue crack growth.
The response of a structure to the effects of cracks is extensively discussed in Chapter 5.

1.5 FATIGUE DESIGN OF TUBULAR JOINTS

The knowledge of the total life of each component in a structure is an essential requirement for the designer. There are various experimental based approaches for calculating the total life of a welded tubular joint. A stress-life (S-N) diagram for offshore tubular joints was first published by the American Welding Society in 1972 (known as the X-Curve) and later revised by the UK Department of Energy (known as the Q-Curve) in their fatigue guidance. It provides a simple guideline for the fatigue design of structures fabricated from welded tubular joints. A further method discussed in this section depends on the initiation life and the estimated propagation life calculated from prediction methods for the response of cracked bodies under load, i.e. the discipline of fracture mechanics.

1.5.1 Fatigue Life Prediction using S-N Curves

This method involves fatigue loading of tubular joints to failure. The value of the hot spot stress is plotted against the number of cycles to failure. Construction of an S-N curve can be very time consuming and costly as each specimen tested provides a single point on the curve. For this reason, the early S-N curves, referred to as T’ design curves for welded tubular joints, were a product of small-scale fatigue tests performed in air on T and K-joints [1.17].

The total fatigue life can be subdivided into three stages: initiation, propagation and final fracture. In the case of tubular joints, a vast majority of the total life is dominated by the initiation and propagation stages. An idealistic approach of combining the initiation life with the propagation life is undertaken when constructing a T’ design curve.
One of the distinct features of an S-N curve is that the points are combined to form a straight line on a log-log scale. A second characteristic is the fatigue limit. This is indicated by a portion of the curve which displays no increase in stress with added fatigue cycles, i.e. no crack initiation takes place. This fatigue limit is removed in certain cases where the tests are performed in an aggressive corrosive environment.

The basic design S-N curve equation is represented in below.

\[ \log(N) = \log K - m \log(\Delta S) \]  

(1.3)

where:

- \( N \) = Predicted number of cycles to failure
- \( K \) = Constant
- \( m \) = Gradient of the S-N curve
- \( \Delta S \) = Hot spot stress range

### 1.5.1.1 Recommended Failure Criteria

An appropriate failure criterion is generally dependent upon the available NDT capabilities. The HSE report OTH 92 390 [1.18] defines the various failure stages as:

- \( N_1 \) first discernible surface cracking as noted by any available method. This stage is considered to have passed if the initial surface length is found to be greater than 20 mm.

- \( N_2 \) intermediate surface cracking as detected by visual examination without the use of crack enhancement fluids or optical aids. However, if NDT techniques indicate a crack length of 30 mm, this stage is considered to have been reached.

- \( N_3 \) first through-wall cracking as detected either visually, or more accurately by loss of internal pressure or by monitoring of the output of strain gauges positioned adjacent to the crack at its deepest part.

- \( N_4 \) end of test occasioned by complete severance of a brace member, extensive cracking leading to loss of load symmetry or exhaustion of actuator stroke.
The third criterion (first through-wall cracking) was used in the formulation of the UK T’ design curve. The stiffness of a tubular joint is reduced by the presence of a fatigue crack, up until the defect has penetrated the chord wall. As the defect grows as a through-thickness crack, the joint stiffness is reduced at a greater rate. This is discussed in greater depth in Chapter 5.

The fourth definition can be discarded in certain cases as a joint having reached this condition is unlikely to be repairable and has more than likely redistributed the load to other members in a redundant structure.

1.5.1.2 Allowance Factors used in Conjunction with the Basic S-N Curve

Various factors can effect the fatigue life of a tubular joint and as such, affect the shape of the T’ design curve. It has long been recognised that the parent plate thickness of the tubular joints has an influence on its fatigue properties [1.19, 1.18]. In general, a thicker parent plate results in a shorter fatigue life. This phenomenon is referred to as the ‘thickness effect’ and can be attributed to two reasons. Firstly, the larger material section is more likely to contain flaws due to poor manufacturing or metallurgical anomalies. The second factor contributing to the ‘thickness effect’ is the lower through-thickness stress gradient for a thicker plate. This implies that a fatigue crack in a thick plate subjected to bending will experience greater stresses than a similarly sized crack in a thinner plate. The HSE provide a correction factor, which is applied as a penalty factor, for stresses on joints with a thickness greater than the 16 mm reference thickness for tubular joints [1.18].

Another factor that needs to be continuously addressed is the effect of the environment on fatigue life. The T’ design curves described above were obtained from the results of fatigue tests performed in a laboratory air environment. The environmental conditions experienced by tubular joints depend on their location within the structure.
- Above the water level. Close to the topside deck the environment closely resembles that of a laboratory with the added presence of salts in the air.
- Near the water level. This region, also known as the splash zone, is subjected to intermittent immersion due to tidal rise and fall as well as wave spraying.
- Constant immersion. This concerns the majority of the structure. The region is in a constant aggressive corrosive environment and needs to be continuously protected.

Vinas-Pich [1.20] and Austin [1.21] performed multi-sea state, variable amplitude corrosion fatigue tests on BS 4360 50D / BS 7191 355D type steel welded tubular joints. Austin found that the corrosion fatigue endurance was, on average, four times less than the mean fatigue life in air. Also, there was no indication of a fatigue limit for stresses as low as 90 MPa.

Etube [1.22] and Myers [1.23] also performed Variable Amplitude Corrosion Fatigue (VACF) tests and Constant Amplitude Corrosion Fatigue (CACF) tests on tubular joints, respectively. These joints were manufactured from a high strength, high quality steel (Superelso 702) which has a yield strength of about 690 MPa [1.24]. Etube found that a fatigue limit indeed existed for his specimens under similar conditions to the tests performed by Austin. A second major difference between the two sets of tests was that the lower strength steel data points all lie below the S-N Air Design Curve, whilst the SE 702 data points all lie above this line. This suggests that the high strength steel joints may have longer fatigue lives at lower stress levels. There is, however, insufficient data to identify a clear trend.

1.5.2 Linear Elastic Fracture Mechanics
The presence of a crack in a structure represents a stress singularity. As the structure is loaded, a small area at the crack tip is plastically deformed due to the high stresses. If this area, known as the plastic zone, is insignificant compared with the crack size and is not adversely affecting the cracked body, linear theory can be put into use to simplify the analysis. The linear analysis of a cracked body is termed linear elastic
fracture mechanics (LEFM). If the plastically deformed area is large, then a linear approach can no longer be used. Elastic-plastic fracture mechanics (EPFM) is then employed and is further discussed at a later stage in this chapter. In general, the yielded area is smaller than the crack size.

The limitation of the S-N approach, including the lack of ability to assess the structural life expectancy of a cracked tubular in service, has resulted in the worldwide use of LEFM. The technique allows for an accurate assessment of the propagation life of a component containing a fatigue crack. This, along with a separate calculation for the initiation life (strain life), can provide the designer with the total joint life.

1.5.2.1 Fatigue Crack Propagation

The concept of using elastic stress concentration factors breaks down when analysing the stresses close to the crack tip. This is due to the fact that the crack tip radius is assumed to approach zero and hence the stresses tend to approach infinity. The LEFM approach avoids this difficulty by analysing certain aspects of the stress field that surrounds the crack.

Cracks grow in a variety of modes described below and further represented by Figure 1.7.

- Mode I  \( \rightarrow \) Tension - normal to the faces of the crack and the most commonly occurring mode (opening mode)
- Mode II  \( \rightarrow \) Shear - normal to the crack front in the plane of the crack (edge sliding mode)
- Mode III  \( \rightarrow \) Shear - parallel to the crack front (tearing mode)

Data obtained under one particular mode cannot be applied to any other modes.

For a linear elastic material, the stress normal to the crack plane, \( \sigma_{yy} \), is defined as:
\[ \sigma_{yy} = \frac{K_1}{(2\pi r)^{1/2}} \quad \text{for } r \ll c \quad (1.4) \]

where:
- \( r \) = Distance from the crack tip
- \( K_1 \) = Mode I stress intensity factor
- \( c \) = Half crack length

The above equation is graphically represented in Figure 1.8. As the value of 'r' increases, \( \sigma_{yy} \) approaches a constant value represented by \( \sigma^\infty \), due to the uniform remote tensile stress.

The stress intensity factor allows for the quantification of the rise in stress as the crack tip is approached. The definition of \( K_1 \) as shown in the above equation is only valid for a two-dimensional centre-cracked infinite plate under a uniform tensile stress, illustrated in Figure 1.9. Modifications are required for other configurations, crack geometries, and loading conditions. The result of these modifications is shown in Equation (1.5).

\[ K = \sigma Y (\pi a)^{1/2} \quad (1.5) \]

where:
- \( \sigma \) = Nominal stress
- \( Y \) = Modification factor
- \( a \) = Crack depth

The modification factor is a dimensionless constant and is dependent on geometry and the mode of loading. For example, an edge crack in a semi-infinite plate subjected to remote tensile stressing is shown in Figure 1.10. The SIF for this geometry is given by:

\[ K = 1.12\sigma (\pi a)^{1/2} \quad (1.6) \]
This factor is usually determined by either experimental or analytical methods for a large number of geometries and loading conditions. A review of all the available methods is not in the scope of this thesis. The methods relevant to the study have been discussed in Chapter 4.

Paris-Erdogan Law
In the 1960s Paris and Erdogan investigated the relationship between fatigue crack growth and the stress intensity factor range. They showed that the fatigue crack could be characterised as a function of $\Delta K$. A plot of $da/dN$ (rate of crack growth) against $\Delta K$ is shown in Figure 1.11 and can be subdivided into three regions.

Region I illustrates a threshold value of the SIF ($\Delta K_{th}$) below which cracks will not propagate. Behaviour in this region is dependent on microstructural features.

Region II represents the zone in which the plot is effectively linear, and hence the curve can be described by the Paris-Erdogan relationship in form of the equation below.

$$\frac{da}{dN} = C(\Delta K)^m$$  \hspace{1cm} (1.7)

where $C$ and $m$ are experimentally determined material constants. Crack growth rates are relatively insensitive to microstructural stress effects.

Region III demonstrates a rapid increase in the crack propagation rate. This region corresponds to the onset of unstable crack growth and is characterised by either the material’s fracture toughness (described in Section 1.5.2.2) or plastic instability.

The use of the Paris Law allows the remaining life of a cracked component to be evaluated in terms of the SIF range $\Delta K$, provided that the crack growth is confined to region II and that there is a suitable expression for $\Delta K$.  

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The number of cycles required (N) to propagate a crack from a given initial to a final crack size (a_i to a_f) can be determined by rearranging Equation (1.7), substituting in Equation (1.5), and integrating between the initial and the final crack sizes. Replacing the stress with the cyclic stress range, the integration becomes:

\[
N = \int_{a_i}^{a_f} \frac{1}{CY^n \Delta \sigma^{m/2} a^{m/2} \pi^{m/2}} da
\]

(1.8)

**Stress Intensity Factors for Tubular Joints**

Determining Y factors for tubular joints has proven to be a complex task. Their irregular shapes give rise to non-uniform stress distributions which could lead to mixed mode loading. Applying plate solutions to evaluate stress intensity factors for tubular joints can lead to very conservative estimates. As a crack in a plate is grown to a large size, the remaining ligament experiences very high stresses, due to the reduction in the load bearing material, thus leading to high local SIF values. In case of tubular joints, the load is transferred to another part of the intersection and as such, the local stresses, and subsequently the SIFs, are lower than for plates.

There are only a limited number of models for determining the SIF at the crack deepest point for tubular joints. An example of the analytical approach is the Haswell SIF solutions [1.25] for cracks at the hot spot location in tubular joints, which are recommended by BS 7910 - *Guide on methods for assessing the acceptability of flaws in metallic structures* [1.26]. Thin shell finite elements were used to model various joint configurations and geometries. Line spring elements were utilised to model the crack. As line spring elements do not provide accurate solutions at the ends of the semi-elliptical crack [1.27], the crack end SIFs were not provided.

Dover et al. provided semi-empirical solutions for surface cracks in T and Y-joints based on the results from an experimental database of fatigue tests [1.28]. The method of calculating the experimental modification factor is outlined in Chapter 4.
Most SIF solutions are for the maximum depth of the crack and as such cannot be incorporated to estimate values for through-thickness cracks. To the author's knowledge, there are no SIF solutions for through-thickness cracks in tubular joints. In this thesis a solution is proposed to estimate the SIF for axially loaded T-joints containing through-thickness cracks, by using a basic two-dimensional plate model and allowing for the non-uniform stress distribution with the aid of a thick shell finite element model. The SIF model is presented in Chapter 4.

1.5.2.2 Fracture Toughness

Fracture toughness is a measure of the resistance of a material to crack extension. Linear elastic fracture mechanics may be applied to predict the conditions that will cause failure for certain simple structural geometries. The critical stress intensity factor, $K_c$, is a function of the global stress applied to the specimen, and the crack length.

The crack may propagate rapidly and in an unstable manner through a relatively thick component. The critical value in this case is called the plane strain fracture toughness. A condition necessary to achieve plane strain is the presence of sufficient elastic constraint at the crack tip. The crack acts as a stress raiser and the stress at the tip of the crack invariably exceeds the yield strength causing the development of a small area of plastically deformed material. This plastic zone is surrounded and constrained by an elastic stress field.

When the section thickness is small compared to the plastic zone size at the tip, significant strain occurs through the thickness direction, and plane stress conditions exist. Therefore, the point of instability is less well defined and depends on the crack tip plasticity, the thickness, and other geometrical factors.

Figures 1.12a and 1.12b illustrate the two states of stress in a cracked plate.
1.5.3 Elastic-Plastic Fracture Mechanics

Linear elastic fracture mechanics is valid only as long as the crack tip plasticity is confined to a small region. In certain situations, it is not feasible to characterise the fracture behaviour with LEFM, and therefore, an alternative fracture model is required. The fracture mechanics approach is extended beyond the validity limits of LEFM by idealising elastic-plastic deformation as non-linear elastic.

The two parameters that describe crack tip conditions in elastic-plastic materials are the crack tip opening displacement (CTOD), and the J-integral, which is used as a fracture characterising parameter for non-linear materials.

Critical values of CTOD or J give size-independent measure of fracture toughness, even for a relatively large amount of crack tip plasticity. In this chapter, only the CTOD is discussed further.

1.5.3.1 Crack Tip Opening Displacement

In the early 1960s Wells [1.29] attempted to measure the fracture toughness values in a number of structural steels. He found that these materials were too tough to be characterised by LEFM. While examining fractured test specimens, Wells noticed that the crack faces had moved apart prior to fracture, i.e. plastic deformation had blunted an initially sharp crack as shown in Figure 1.13. The degree of crack blunting increased in proportion to the toughness of the material. This observation led Wells to propose the opening at the crack tip as a measure of fracture toughness, known today as the CTOD. He then hypothesised that the CTOD is an appropriate crack tip characterising parameter when LEFM is no longer valid.

Experimental methods for measuring CTOD range from utilising a flat paddle gauge inserted into the specimen, to the more commonly used clip gauge, which is an instrument that allows the measurement of the displacement at the crack mouth. The CTOD is then calculated by assuming the specimen halves are rigid and rotate about a
hinge point. Tests have generally been performed on edge-cracked specimens loaded in three-point bending as illustrated in Figure 1.14.

1.5.3.2 The CTOD Design Curve

The British Welding Research Association (currently The Welding Institute) applied the CTOD concept to structural steels in the late 1960s. For linear elastic conditions, the fracture mechanics theory was well established, but the theoretical knowledge required to estimate the driving force under elastic-plastic and fully elastic conditions did not exist until the late 1970s. In 1971, Burdekin and Dawes [1.29] developed the CTOD design curve based on Wells’ original idea that global strain should scale linearly with CTOD under large-scale yielding conditions. Burdekin and Dawes based their elastic-plastic driving force relationship on Wells’ suggestion and an empirical correlation between small scale CTOD tests and wide, double-edge notched tension panels made from the same material. The wide plate specimens were loaded to failure, and the failure strain and crack size of a given large-scale specimen were correlated with the critical CTOD in the corresponding small-scale test.

This resulted in the CTOD design curve, illustrated in Figure 1.15. The critical CTOD, \( \delta_{\text{crit}} \), is non-dimensionalised by the half-crack length, \( a \), of the wide plate and is represented on the ordinate of the graph. The non-dimensional CTOD is plotted against the failure strain in the wide plate, \( \varepsilon_f \), normalised by the elastic yield strain, \( \varepsilon_y \).

The applied strain and flaw size in a structure, along with the critical CTOD for the material can be plotted on Figure 1.15. If the point lies above the design curve, the structure is considered safe because all observed failures are below the design line.

1.5.4 Ductile-Brittle Transition in Steels

There have been many instances in the past of failure of metals by unexpected brittleness at low temperatures. Metals that suffered normal ductile fracture at ambient temperatures would fail at low temperatures by sudden cleavage fracture.
at comparatively low stresses. The failure of the motor sledges during the very early stages of the British South Pole expedition of 1912-1913 was attributed to fracture of this type and contributed towards the final disaster which overtook the Polar party [1.30]. Around the same period, the Titanic passenger ship sank in the Atlantic Ocean due to a collision with an iceberg. It has been reported that the ductile-brittle transition temperature was found to be 20°C in one direction and 30°C in the other, compared with about -15°C for modern steels [1.31]. This would indicate that the fracture of the hull due to a high impact load, in sub-zero temperature waters, was a likely possibility. More recently, similar failures were experienced in the welded Liberty ships manufactured during the Second World War [1.32]. The hull of the ships underwent a ductile to brittle transition due to the low temperatures of the North Atlantic. This resulted in the hull being literally torn in half by a rapidly propagating brittle crack induced by a relatively small impact loading, together with the residual stresses present due to welding. Other similar examples have been observed in bridges, pressure vessels and gas transmission pipe lines.

The fracture toughness of most steels can change drastically over a small temperature range as shown in Figure 1.16. At low temperatures failure by brittle cleavage fracture is more common. At higher temperatures, ductile tearing is present and the steel fails by the coalescence of microvoids. Ductile fracture is initiated at a particular toughness value as indicated in Figure 1.16. The crack propagates with an increase in load and eventually, the specimen fails by plastic collapse. Both types of fracture can be present in the transition region. In the lower transition region, the fracture mechanism is pure cleavage, but the toughness increases rapidly with an increase in temperature, as cleavage fracture becomes more difficult. In the upper transition region, cracks initiate by the merger of the microvoids, but the ultimate failure occurs by cleavage.
1.6 STATIC CAPACITY OF TUBULAR JOINTS

Tubular joint connections need to be designed to be able to resist the maximum predicted static loads during their service life. The design calculations are based on mathematical expressions derived from extensive experimental results, which can be expressed in limit state terms. For a static strength analysis, permissible loads are based on interpretations of ultimate load test data combined with a suitable safety factor. The equations can be refined with the addition of more test data. Furthermore, different formulae may be proposed for the design of the same component, depending on the choice of the database or the screening criteria. Several sets of expressions for calculating the ultimate strength of tubular joints are presented in this section.

Pan, Plummer and Kuang Equations
This set of expressions are based on data from 214 K-joints and 132 T, Y, and X-joints, loaded under either axial tension or compression. The tests were performed on mainly small-scale specimens. No safety factors were applied in this formulation, indicating that the static strength values obtained may be non-conservative. Also, the material strength term is based solely on the yield strength. This does not allow for the utilisation of the post-yield properties of tubular joints. The equations for T, Y, and X-joints and the corresponding limits are presented in Appendix A.1 [1.33].

API RP2A Equations
Two sets of equations, the working stress design (WSD) and the load and resistance factor design (LRFD), are provided by the API RP2A documents. Both sets are based on the test results of 137 static strength tests on simple T, Y, DT, and K-joints under different modes of loading, as reported by Yura [1.34]. Most tests were conducted on small-scale models. The predictions correspond to the observance of first cracking.

For most existing structures working stress principles apply. Design loads, for example from a 100-year storm, are applied to the structure and the forces and moments in the components are compared with the limit values from an appropriate
design guidance, which includes a single factor of safety. Load and resistance factor design codes relate to the adequacy of a component to withstand its intended load. The WSD equations and the LRFD equations for the ultimate capacity of tubular joints are provided in Appendices A.2 and A.3, respectively [1.35, 1.36].

**HSE Equations**

The HSE equations for the ultimate limit state of tubular joints are based on maximum load from a database of over 200 test results on mainly smaller scaled specimens, many of which were used in the API database. The implications of using data from small-scale tests are discussed in Section 1.7.1. These equations generally provide higher design loads when compared with the API equations. Also, the material strength term in both sets of equations, that being the lower of the yield strength or 2/3 of the ultimate tensile strength, does not allow for the maximisation of potential benefits of modern high strength steels, which may have high yield to ultimate strength ratios. The empirical formulae are provided in Appendix A.4 [1.6].

The API and the HSE equations are the two most recent and commonly employed sets of equations for the calculation of the ultimate capacity of tubular joints.

Reference 1.37 reports of a set of equations in the forthcoming ISO document [1.38], which incorporate a substantially larger database than either the API or the HSE equations (659 steel tubular joints). They are reportedly based on the API LRFD format. This document had not been published at the time of this writing [1.39].

### 1.7 STATIC STRENGTH OF CRACKED TUBULAR JOINTS

While equations to predict the static strength of tubular joints are a useful design tool, there is still a requirement for an analysis method to estimate the ultimate capacity of cracked components. Figure 1.17 shows the relationship between the normalised load
ratio and the crack area, expressed as a percentage of joint cross-section, for large-scale and small-scale steel specimens, finite element predictions, and small-scale tin-lead alloy specimens [1.37]. The normalised load is presented in terms of the ratio of the static strength for a cracked specimen to the corresponding predicted static strength for the uncracked specimen, using the HSE characteristic load. The figure shows that there is an approximately linear relationship between normalised static strength and crack size. A summary of the origins of the various sets of data is provided in Reference 1.40.

Also shown in Figure 1.17 is the reference line plotted by using the BS 7910 procedure [1.26] for the prediction of the static strength of cracked tubular joints. The procedure involves applying a dimensionless area reduction factor to the uncracked joint capacity to allow for the loss of load-bearing intersection area due to the presence of the flaw. The reduction factor, \( F_{AR} \), is given by:

\[
F_{AR} = \left( 1 - \frac{A_c}{Bl_w} \right) \left( \frac{1}{Q_\beta} \right)^{m_q}
\]  

(1.9)

where:

- \( A_c \) = Crack area (surface crack length \( \times \) plate thickness for through-thickness defects)
- \( B \) = Section thickness in plane of flaw
- \( l_w \) = Intersection weld length
- \( Q_\beta \) = Accounts for the increased strength for \( \beta > 0.6 \)

and:

- \( Q_\beta \) = 1 for \( \beta \leq 0.6 \)

or:

- \( Q_\beta \) = \( 0.3/[(\beta(1-0.833\beta)] \) for \( \beta > 0.6 \)
- \( m_q \) = 1 or 0 (depending on the design equations used)

It can be seen that almost all data points satisfy the simple relation that the static strength of a cracked tubular joint can be conservatively estimated by utilising the BS
7910 approach. To the author’s knowledge, this technique has not been correlated with any tubular joints manufactured from high strength steels.

It is of great importance to recognise that the BS 7910 procedure is solely based on plastic collapse behaviour and takes no account of fracture. It is likely that most specimens used to plot the data on Figure 1.17 exhibited a certain amount of extension of the initial crack by fracture. It can therefore be argued that correlation of the initial crack size with the maximum load is also inaccurate. Furthermore, a large number of the tests have been performed on small-scale specimens. The following section depicts the potential problems of relying on data from small-scale tests as well as the need for full-scale testing.

1.7.1 Small-Scale Testing

While it is recognised that the failure modes of tubular joints experiencing static loading are generally dominated by gross, plastic ovalisation of the chord, and that the small-scale factors will not be as severe as in case of joints subjected to fatigue loading, these effects should not be ignored.

Effects of the Weld

A number of the experimental data displayed in Figure 1.17 were from cast specimens and had no representation of the weld. The effects of the weld angle and the weld toe radius were briefly discussed in Section 1.4. The magnitude of notch stresses is directly related to the quality of the welding. Great care is taken to reduce the severity of change in section when a brace is welded to a chord. This would be difficult to achieve on a small-scale specimen.

Crack Size

The crack tip conditions are more severe for a crack in a large-scale specimen than that of a small-scale specimen, of the same geometry, under similar stress levels.
Through-Thickness Stresses
Specimens with a chord wall thickness as small as 5 mm were part of the testing programme [1.41]. The through-thickness stress gradients for these specimens are much higher than, and thus not representative of, a full-scale specimen.

1.7.2 Full-Scale Testing
Although more costly and complex to perform, full-scale testing has several indispensable advantages, the most apparent of which being that it is the closest representation of in-service conditions. Also, it enables feedback of information to the designer on the areas of crack formation and modes of deformation, to verify both the validity of the calculation methods and their basic assumptions. Furthermore, it allows better visualisation of the failure of a member and as such it should be used when an aspect of the safety of a structure needs to be investigated.

1.8 FAILURE ASSESSMENT DIAGRAM

The importance of the CTOD design curve in considering crack tip failure and brittle fracture was briefly highlighted in Section 1.5.3. This, however, takes no consideration of failure by plastic collapse in the case of overloading. An approach suggested by Dowling and Townley in 1975 [1.42] considered failure by linear elastic fracture as one limiting criterion and failure by plastic collapse as the second. A design curve is used to interpolate between the two failure criteria. This work was continued the following year by Harrison et al. [1.29] who attempted to represent the interaction of fracture and collapse. Their work spawned two assessment approaches, PD 6493 [1.43] and the R6 method [1.44]. Both methods are based on failure analysis using a Failure Assessment Diagram (FAD). The FAD relies on two dimensionless parameters, the stress intensity ratio, \( K_r \), and the stress or load ratio, \( S_r \) or \( L_r \). When performing a structural integrity assessment of a flaw in a stressed structure, the coordinate of an assessment point is plotted on the FAD. If the point calculated lies on
the curve or falls outside it, the structure is deemed unsafe, while if the point is within the curve, no failure is predicted.

Brittle fracture is predicted when the applied stress intensity factor, $K_I$, equals the critical stress intensity factor, i.e. the fracture toughness. The fracture axis is common to all types of FAD approaches and is defined by the stress intensity ratio, $K_r$ as:

$$K_r = \frac{K_I}{K_{IC}}$$ (1.10)

where: $K_I$ = Mode I stress intensity factor
        $K_{IC}$ = Mode I fracture toughness

1.8.1 BS 7910

The PD 6493 procedure for the assessment of defects in welded components, originally published in 1980 and subsequently revised in 1991, has been used with some success in industry and is now applied extensively to offshore structures [1.45]. Its main applications are the fitness-for-purpose assessment of fabrication and in-service defects, inspection scheduling and the determination of whether or not post weld heat treatment is required. PD 6493 was further revised as a result of work done by The Welding Institute in collaboration with the British Standards Institution as BS 7910 [1.26], which refines the approaches of PD 6493 but retains the same principles.

There are three levels of assessment depending on the input data available, the level of conservatism and the degree of accuracy required.

Level 1

Level 1 is the screening level introduced into the 1991 PD 6493. It provides a conservative estimate using a simplified FAD with in-built safety factors and requires conservative estimates of the applied stress, the residual stress and the fracture toughness input. The curve for this assessment level is shown in Figure 1.18.
This level of assessment is consistent with the CTOD design curve in the 1980 version of PD 6493. For CTOD data, $K_r$ is replaced by $\sqrt{\delta_r}$ and is defined as:

$$\sqrt{\delta_r} = \frac{\delta_t}{\delta_{crit}}$$  \hspace{1cm} (1.11)

where $\delta_t$ is the applied CTOD obtained from a modified form of the CTOD design curve [1.29].

Equations (1.12) and (1.13) show the relationship between $\delta_t$ and $K_r$.

$$\delta_t = \frac{K_t^2}{\sigma_y E} \quad \text{for } \sigma_{max}/\sigma_y \leq 0.5$$  \hspace{1cm} (1.12)

$$\delta_t = \frac{K_t^2}{\sigma_y E} \left( \frac{\sigma_y}{\sigma_{max}} \right)^2 \left( \frac{\sigma_{max}}{\sigma_y} - 0.25 \right) \quad \text{for } \sigma_{max}/\sigma_y > 0.5$$  \hspace{1cm} (1.13)

The plastic collapse criterion was defined as the ratio of the net stress to the flow strength in the PD 6493 document. Plastic collapse occurs when this ratio is equal to 0.8. The stress ratio, $S_r$, can be defined as:

$$S_r = \frac{\sigma_{net}}{\sigma_{flow}}$$  \hspace{1cm} (1.14)

where:

$$\sigma_{net} = \frac{\text{Applied Load}}{\text{Remaining Cross - Sectional Area}}$$  \hspace{1cm} (1.15)

$$\sigma_{flow} = \frac{\sigma_{uts} + \sigma_{yield}}{2}$$  \hspace{1cm} (1.16)
Annex P in the BS 7910 document includes a substantial number of net section stress solutions, $\sigma_{\text{ref}}$, for many geometries containing either through-thickness or partial-thickness defects. Therefore, Equation (1.14) has been changed to:

$$S_i = \frac{\sigma_{\text{ref}}}{\sigma_{\text{flow}}} \quad (1.17)$$

**Level 2**

This is considered to be the normal assessment route for general structural steel application and makes use of the FAD with no extra safety factors and thus is a more accurate and less conservative alternative to Level 1. It has two methods, 2A and 2B, each having its own Failure Assessment Diagram and a cut-off value. One of the main differences between Level 1 and Level 2 is that the former is based on the assumption of an elastic-perfectly plastic stress-strain relationship, with no allowance for work hardening. In Level 2 (and Level 3) analysis, the plastic collapse axis is represented by $L_r$ to compensate for this conservatism.

The stress ratio has been replaced by the load ratio, and is given in PD 6493 as:

$$L_r = \frac{\sigma_{\text{net}}}{\sigma_{\text{yield}}} \quad (1.18)$$

and was subsequently updated in BS 7910 to:

$$L_r = \frac{\sigma_{\text{ref}}}{\sigma_{\text{yield}}} \quad (1.19)$$

Figure 1.19 shows a Level 2A FAD. The equation describing the line is as follows:

$$K_i = (1-0.14L_r^2)[0.3 + 0.7\exp(-0.65L_r^6)] \quad (1.20)$$
This can be used when the stress-strain curve for the material is not available, such as would be the case if the heat affected zone of a weld is analysed.

As the plastic collapse axis is normalised to the yield strength and not to flow strength, the assessment curve extend beyond unity and represents material strain hardening as seen in Figure 1.19. The cut-off load ratio is represented by $L_{\text{max}}$. It requires only knowledge of the yield and tensile strengths of the material and is given by the following expression:

$$L_{\text{max}} = \frac{\sigma_{\text{flow}}}{\sigma_{\text{yield}}}$$  \hspace{1cm} (1.21)

Different values of $L_{\text{max}}$ are used for various steels due to the alternative strain hardening characteristic of the materials.

Level 2A analysis was used for all assessments in this study.

Level 2B requires a specific stress-strain curve for the material and will generally provide more accurate results than Level 2A. A sample Level 2B curve is shown in Figure 1.20 and the corresponding stress-strain curve is given in Figure 1.21 [1.26].

**Level 3**

This level employs a full tearing instability approach and provides a more accurate description of ductile materials. It consists of 3 methods, Levels 3A, 3B, and 3C. This level was outside the scope of this thesis.

**1.8.1.1 Global Collapse Analysis**

Until this point, the load ratio, referred to as the collapse parameter in BS 7910, has been analysed for local collapse analysis. BS 7910 and other literature [1.46] have discovered that the local collapse approach is usually very conservative and has led to
a wide scatter of results, while the global collapse approach provides more realistic prediction of plastic collapse in tubular joints. The collapse parameter is given by:

\[ L_i = \frac{\sigma_L}{\sigma_y} \left( \frac{P_a}{P_c} + \frac{M_{ai}}{M_{ci}} \right)^2 + \frac{M_{ao}}{M_{co}} \]  

(1.22)

where \( P_a, M_{ai} \) and \( M_{ao} \) are the applied axial load, and the in-plane and out-of-plane moments, respectively. \( P_c, M_{ci} \) and \( M_{co} \) are equal to the product of the \( F_{AR} \) (from Section 1.7) and the yield strength based collapse loads and moments for the uncracked components, respectively.

The global collapse approach will be used in Chapter 4 to evaluate the FAD parameters for all tubular joint specimens.

1.8.2 The R6 Method

As mentioned previously, the keystone of the R6 procedure is also the Failure Assessment Diagram. The method incorporates the same principles as the Level 3 of the BS 7910.

1.9 REQUIREMENT FOR HIGH STRENGTH STEEL GUIDANCE

Weldable high strength steels, generally defined as steels with a minimum yield strength of 450 MPa, can provide many benefits for the offshore industry. Their excellent strength to weight ratios have been responsible for large reductions in the total weight and subsequent decrease in the overall costs of offshore structures.

One area where the use of high strength steels is well established is in the construction of Jack-up platforms. These steels are used to manufacture the rig legs as well as the rack and spud cans. More recently, however, there has been a considerable growth in
incorporating high strength steels in the construction of lighter jacket structures [1.47]. With the evolution of heavy lifting crane barges, lift installed platforms have become an increasingly attractive option when compared to barge loaded structures.

As is usually the case with the use of a new material to build any engineering structure, a certain amount of time is required in order to earn the confidence of the industry. During this period, many tests are performed and the potential pitfalls are identified in the relevant design guidance codes.

There is a distinct lack of published data for welded tubular joints made from higher strength steels and thus design codes on high strength steels used in the offshore industry have also been limited. Most published codes and standards are only available for medium strength steels such as the commonly used BS 4360 50D / BS 7191 355D. There is currently an effort being made to develop an International Organization for Standardization (ISO) standard for offshore installations [1.48] as an evolutionary improvement to the now withdrawn Health and Safety Executive Guidance notes [1.6]. It is hoped that the ISO document can add to the many positive aspects of the HSE Guidance notes by addressing its shortcomings, especially regarding the use of tubular joints manufactured from high strength steels.

1.9.1 Yield Strength to Ultimate Strength Ratio

It is known that high strength steels have different stress-strain characteristics from lower strength steels. This is illustrated in Figure 1.22 for a conventional strength structural steel with yield strength, $\sigma_y$, of 350 MPa, and a higher strength steel with a yield strength of 450 MPa [1.49]. The higher strength steel has a smaller stable plastic region when compared with the medium strength steel.

The post yield behaviour can be quantified by considering the ratio of the yield strength to the ultimate tensile strength, $\sigma_y/\sigma_{uts}$, of both steels. A higher $\sigma_y/\sigma_{uts}$ (0.85) for the higher strength steel indicates a reduction in the amount of work hardening and thus a diminished safety margin. The $\sigma_y/\sigma_{uts}$ is intended to provide a margin of
strength against accidental overloading of the members. In the event of an overload, steels with a lower $\sigma_y/\sigma_{ult}$ may work harden and resist the load imposed on the structure. The plastic deformation may also allow for the redistribution of the load. Steels with higher ratios may undergo complete separation if overloaded and could possibly not allow for the discovery of the initial defect during a scheduled inspection. They do, however, provide a larger elastic region. It is up to the designer, with the aid of the relevant certifying authorities, to maximise the potential of the higher strength steels.

An assessment of the HSE guidance [1.6] and the American Petroleum Institute’s Recommended Practice (API RP) 2A [1.35, 1.36] shows that the design stress in nodal joints is limited to the lesser value of the yield strength or approximately 2/3 of the ultimate tensile strength. As mentioned previously, high strength steels generally have a high $\sigma_y/\sigma_{ult}$ and as such the penalty of the above restriction is more severe than that of a medium strength steel. The codes imply that effectively, there is nothing to be gained by using a high strength steel with a $\sigma_y/\sigma_{ult}$ of more than 0.67.

The restrictions from the codes were generally introduced to ensure a safe practice and were not based on the results of a large study into the behaviour of tubular joints. While it is desirable to ascertain a certain amount of work hardening, the "the lesser value of the yield strength or approximately 2/3 of the ultimate tensile strength" limit should vary for different generations of steels to allow for the maximum achievable benefits. This can mainly be achieved through knowledge and experiences gained from performing a large range of tests.

1.9.2 Factors Affecting the Utilisation of High Strength Steels

While the use of high strength steels is becoming more common, there is still a fear in the offshore industry about the performance of these steels in severe environmental conditions.
In 1992 a paper by the Health and Safety Executive, Offshore Safety Division reported on cracking around the spud cans of Jack-up platforms operating in the North Sea [1.4]. The cracks were thought to have been hydrogen assisted cracks in the heat affected zone and were likely caused by the combination of excessive cathodic protection (CP) voltage levels, high residual stresses, and the general susceptibility of high strength steels to the effects of hydrogen. These findings were verified by a set of slow strain rate tests in air and various compositions of synthetic seawater using samples from the components cracked in service. The study concluded that the possibility of hydrogen induced cracking in high strength steels can be significantly reduced by controlling its three main contributors.

- Generation of hydrogen by excessively negative CP potential levels → This can be avoided by using voltage regulators or voltage limiting diodes.
- Susceptibility of high strength steels to hydrogen assisted cracking → All high strength steels should be pre-qualified for use on the basis of experiments in appropriate environmental conditions.
- Presence of residual stresses → Designers should pay close attention to the various fabrication processes, including cold forming and welding, which may cause residual stresses (if not stress relieved).

Aside from metallurgical implications, the designers must take into consideration the fatigue strength of high strength steels. This is becoming a major concern as Jack-up platforms are to an increasing extent being considered for use as long-term production support structures. The implications of this new application for structural integrity assessment and maintenance are vast. Traditional regular dry dock inspection and repair is no longer possible when a platform is stationed for medium to long term production support periods. For this reason, the extent to which Jack-up platforms can tolerate fatigue damage must now be a design consideration. Also, Jack-up platforms by their nature do not have the degree of structural redundancy as found in traditional fixed jacket production structures. This means that the static strength capacity of Jack-up platforms is far more sensitive to reduction in strength due to fatigue cracking
in individual members than equivalent cracking in jacket structures. Hence, it is recommended to further investigate the fatigue behaviour and the residual static strength of tubular joints manufactured from high strength steels to obtain a thorough understanding of their defect tolerance capabilities. The residual static strength of high strength steel tubular joints containing through-thickness cracks is investigated in Chapter 2

1.9.3 Verification Studies
As with any other industry which takes certain steps to validate the use of a new material, high strength steels have been subject to a moderate amount of testing. The following section is aimed at illustrating the behaviour and capabilities of a commercially used high strength steel by citing examples of testing and analyses performed under controlled conditions.

Both BP’s Harding Field platform and TotalFinaElf’s Elgin Field platform are manufactured from a high strength steel with a yield strength of approximately 700 MPa. The steel, Superelso 702, is manufactured by Creusot Loire Industries in France. Due to the fact that it is already in common commercial use, several studies have been performed over the past few years to validate the utilisation of this steel and to examine its metallurgical structure, fatigue strength, and its susceptibility to hydrogen induced cracking.

Metallurgical Structure
It is generally considered that high strength steels have a lower toughness than conventional offshore structural steels. In recent years, there has been considerable developments in steel metallurgy, including lower carbon levels, finer grain structures, and greater cleanliness. These have resulted in improved combinations of properties such as strength, toughness and welding performance.

An independent study was conducted at Cranfield University to verify the manufacturer’s claims and the cleanliness of Superelso 702 (SE 702). The steel was
found to have a uniform microstructure and very few inclusions and impurities. Perhaps more importantly, considering the previous study mentioned [1.4], the heat affected zone microstructure was depleted of significant carbide particles or coarse structures that might promote enhanced crack growth behaviour. This study is reviewed in Reference 1.50.

**Fatigue Performance**

The S-N curves for tubular joints in the UK design guidance [1.18] were produced for steels of yield strengths below 400 MPa for tubular joints. Therefore, data from an approved test programme or an associated fracture mechanics analysis needs to be used to determine the appropriate S-N curves for tubular joints manufactured from high strength steels [1.51].

Etube et al. [1.24] performed two sets of full-scale fatigue testing programmes on tubular joints manufactured from SE 702, under constant amplitude loading and variable amplitude loading. The aims of the project were to analyse the fatigue performance of welded SE 702 members, both in air and synthetic seawater under cathodic protection, and to compare the results with standard medium strength structural steel data. The tests illustrated that the fatigue life of tubular joints manufactured from SE 702 is not inferior to that of conventional structural steels. However, the limited number of tests does not allow for a clear pattern to be established. It was recommended that further tubular joint fatigue tests be carried out on SE 702, and other high strength steels, to better quantify the potential benefits.

**Hydrogen Induced Cracking**

By controlling the metallurgical properties, certain modern high strength steels have been produced which have hydrogen embrittlement susceptibilities that are as low as those of BS 4360 Grade 50D steels [1.48]. This, however, is not always the case. Each type of steel should be considered individually and subjected to a rigorous testing programme, particularly in environments and locations where there is a large presence of hydrogen, before being utilised for service.
Coudreuse et al. [1.52] performed a series of experiments to assess the hydrogen cracking resistance of SE 702 when compared with BS 4360 Grade 50D, which is not considered to be sensitive to hydrogen assisted cracking under cathodic protection. Slow strain rate tests (SSRTs) were carried out on cylindrical tensile specimens at different strain rates and in both air and synthetic seawater. The results show the damaging influence of a decrease in the cathodic protection potential, decrease in strain rate, and increase in hydrogen activity mainly caused by the presence of hydrogen sulphide (H$_2$S). They also illustrate the fact that SE 702 is not more susceptible to hydrogen induced cracking that a conventional, medium strength steel. This fact was also substantiated by Myers [1.23] who did not observe any evidence of hydrogen embrittlement during his limited fatigue testing programme on tubular joints manufactured from SE 702.

1.10 INSPECTION REPAIR MAINTENANCE

The offshore industry currently requires that the structural integrity of offshore platforms is ensured by inspecting periodically. This could be accompanied by a repair process of the defect or the damaged member detected together with a maintenance programme to ensure the reliability of the repaired member. The main area of concern is the supporting substructure rather than the superstructure (topside) and thus the designer should make allowances for future sub-sea tasks which may be required during the life of the structure. Any employed Inspection, Repair, Maintenance (IRM) methodology necessitates satisfaction of current and possible future legislation and guidance of the local authorities. The immense cost of offshore inspection and maintenance work has resulted in greater consideration for IRM during the earlier design stages, i.e. design for maintenance.

The safety of a deep-water installation should be ensured by adequate design for its entire life. This should be backed up by continuous monitoring of the main functions and critical components, along with periodic inspection and maintenance or
replacement of items as necessary. Designers should consider the limitations of the current state of the art inspection methods and maintenance procedures in order to design them, as much as possible, out of the system. They should also help identify the prime risk areas of the deep water system so that ready access for monitoring, inspection and ease of maintenance can be considered during the design.

1.10.1 Damage Classification

The presence of various types of loading along with the environmental conditions results in damage to the structure. There are various modes and mechanisms that lead to defects and subsequent failures, and thus it is vital for a designer to be aware of the most common types of deterioration. Factors governing various failure modes such as corrosion, fatigue cracking and marine growth have been discussed in Section 1.3.

1.10.2 Performance Monitoring

Constant monitoring of critical members and perhaps a representative selection of connections from different parts of the structure is required to, for instance, identify and predict fracture of a member. Stacey [1.53] and Sharp et al. [1.54] report that general visual inspection and flooded member detection have accounted for the detection of most significant defects.

Flooded member detection (FMD) is a passive inspection method widely applied to fixed offshore jackets for the detection of through-thickness defects, i.e. when the member has reached the N3 criterion. The basic principle behind the technique is the detection (or non-detection) of water inside the members. The FMD system is attached and interfaced to a Remotely Operated Vehicle (ROV) or controlled directly by a diver. There are two technologies currently used in the offshore industry, based on either radiography or ultrasonics.

The first method involves using a gamma radiation source and a sensitive detector unit on opposite forks positioned across the diameter of the member under inspection. The presence of water within the member will affect the level of
radiation and thus indicate a through-thickness defect. This technique is almost always used from an ROV due to the radiation hazards to divers. Figure 1.23 illustrates the basic principle of this method [1.55]. The second method is illustrated in Figure 1.24 [1.55]. The beam generated by the ultrasonic probe will be transmitted across the member and reflected from the far wall only if the volume under inspection is filled with water.

Another method used for structural monitoring is vibration monitoring using accelerometers. Vibration monitoring systems are most effective on simpler structures with a smaller number of legs and limited redundancy as the severance of a single member will have a more significant effect than for example on a sixteen leg, multi-braced structure. The performance of this monitoring technique can be hindered by the presence of operational noises which will make it difficult to establish a base line signature. Vibration monitoring systems primarily aim to detect and measure significant changes in structural response due to stiffness changes [1.56, 1.57].

A more current, recently employed technique is stress monitoring. This method can be used in conjunction with vibration monitoring. There are several stress monitoring methods that are currently being utilised by the industry or are under the final development stages. Strain gauges are the classic, most widely used source of stress monitoring and measurement. However, a major problem arises when the gauges are placed at great water depths. The connecting wires from the gauges to a strain monitoring system have to be of a considerable length. This will enhance the chances of accidental damage as well as increasing the resistance of the wires. To overcome these problems, a remote stress monitoring system has been designed and developed by Brennan [1.58]. Strain gauges are attached to a battery operated, data storage and analysis device. The data is then retrieved periodically.

Another non-contacting technique that is under development is Alternating Current Stress Measurement (ACSM) [1.59, 1.60]. It originates from the Alternating Current Field Measurement (ACFM) inspection technique [1.61]. The technique is capable of
recording changes in both static and dynamic stresses and is based on the physics of magnetic domain wall motion. An example of the successful deployment of ACSM is reported by Brennan [1.62]. The integrity of an anode clamp was assessed by monitoring the stress in the threaded studs, which secure the clamp to a node, and comparing it to stresses obtained from strain gauges.

Corrosion is a constant source of damage for an offshore installation. There are several established methods to combat corrosion, with the most widely used being cathodic protection (CP). The CP current field needs to be monitored so that there is little or no change in the field value. This can be conducted by a series of strategically placed field measuring instruments. There is requirement for the value of the field to be exactly as those specified in the guidelines. A greater value than the recommended amount could lead to over protection and consequently hydrogen embrittlement. Conversely, a smaller negative potential will result in inadequate protection. Constant monitoring of sacrificial anodes is another requirement. These need to be inspected at regular intervals as they deteriorate with time.

1.10.3 Inspection Planning

In-service inspection by divers or ROVs is carried out to monitor the general condition of structures and detect damage caused by various operational and environmental loading. Discovery of structural damage may lead to repair and/or more frequent inspections. Non-discoveries, however, are also important; they can indicate a higher perceived reliability for the structure. In-service inspection plans are normally based on a range of criteria including fatigue sensitivity, static strength, redundancy and any history of previous fatigue problems.

Inspection is a costly exercise which must be scheduled to avoid other conflicting operations on the platform and also can only be undertaken during periods of good weather. It will be impractical to perform a comprehensive inspection of the entire structure every year and it is common for detailed inspection to be concentrated on a particular area of the platform for operational reasons. The traditional approach is to
carry out a general visual inspection of the platform every year. Inspection effort using sophisticated NDT methods is concentrated on the primary or critical nodes, together with parts of the platform which are known to be at risk of damage, for instance due to boat collisions.

NDT methods for inspection of offshore platforms are constantly being developed due to the need for accurate and reliable information on the detection and sizing of existing defects in order to avoid unnecessary repairs. The selection of the NDT method depends on the experience of the operator and the specific guidance issued in each country by the certifying authorities.

Inspection methods range from General or Close Visual Inspection, which are now normally carried out by ROVs, to specific methods for detecting or sizing cracks.

Visual inspection of offshore structures, performed by either taking photographs or in video format, are often used for surveying less critical areas. Their importance, however, must not be understated as it has been reported that General Visual Inspection and flooded member detection have accounted for the detection of the highest number of significant failures [1.53, 1.54].

In the remainder of this section, more advanced detection and inspection techniques will be discussed.

**Magnetic Particle Inspection**

Magnetic Particle Inspection (MPI) is primarily a detection technique. It can, however, also be used to estimate the surface length of a defect. Flourescent liquid containing ferromagnetic particles in suspension is applied to the component. When a strong magnetic field is induced around an area thought to contain a defect, the particles cluster around the disturbances in the field, most commonly a crack. By using an ultraviolet light source, the particles can be viewed and thus the defect length and position can be known [1.63].
**Alternating Current Techniques**

Alternating Current Potential Drop and Alternating Current Field Measurement methods (ACPD and ACFM) depend on the “skin effect” which arises when alternating current (either injected or induced) flows in a conductor and the associated varying magnetic field confines the current to a layer near the surface. In case of ACPD, the potential drop between electrodes is measured across a surface interrupted by a crack and compared with an uncracked surface. The path length is proportional to the potential drop measured. ACFM uses the concept that a defect in a component will have a measurable effect on the magnetic field generated by the induced current. There is no pre-inspection requirement for the removal of paint and other coatings. This can be particularly important for the operators as there have been well documented cases where the removal of paint in order to perform other underwater inspections of the weld, has led to severe cracking at the brace to chord intersection [1.4]. Both AC techniques can be used for detection and sizing although in most cases they are utilised as complementary sizing methods to MPI [1.61, 1.64].

**Ultrasonic Inspection**

A probe is used to emit a high frequency sound wave into a component, which is in turn reflected by disturbances including cracks and voids. The time taken for the transmission and reflection of a pulse can be interpreted as a distance through the component, and hence allow the defect to be located. The main types of ultrasonic waves are longitudinal, transverse (shear), and surface waves. Wave propagation properties are directly related to the elastic properties of the medium and the relative size of the component. Velocities of various wave types are determined by modulus, density, and Poisson’s ratio for the particular material in which they are propagating. Ultrasonic technique has the major advantage over the previously mentioned methods in that sub-surface defects can be monitored. Also, the same transducer can be used to both generate and detect the ultrasonic waves. Furthermore, The technique can be utilised as a thickness measurement method to assess the remaining material after interaction with a corrosive environment [1.63, 1.65].
**Eddy Current Methods**

The technique is based on the principle of electromagnetic induction and is concerned with the interaction of defects with an alternating magnetic field. A small coil is used to induce eddy currents in a metallic component. These currents produce an alternating field, which opposes the original field and can be detected by the change in electrical impedance of the coil carrying the original field. Changes, such as cracks, in the material under test cause changes in the magnitude and phase of the induced current. This can be detected by suitable instrumentation connected to the first coil or by special sensing coils. Flaw sizing may be achieved by using comparison of the signals against a calibrated specimen. Eddy current NDT is non-contacting and can be used for corrosion damage measurements and weld inspection. [1.63].

**1.10.4 Repair and Maintenance**

Once the inspection of a damaged member is completed and the affected area and the defect type are clearly identified, a suitable repair method needs to be selected. The fourth edition of *Offshore Installations: Guidance on design, construction and certification* [1.6] defines repair as “Work done to an offshore installation in response to damage, in order to restore its structural integrity and to maintain the validity of the Certificate of Fitness applying to that installation”.

Repair, especially in deep water, can be costly due to diving and supply requirements, the use of ROVs, waiting on weather, and other possible time delays resulting in stoppage of platform operation. It is, however, the less expensive and most likely less time consuming alternative to member replacement. The supervising engineer’s decision as to which repair method should be implemented is governed by the member size, joint detail, accessibility problems, and most importantly, the stage of advancement of the defect. For example, if a crack has grown most of the way through the thickness, a significant increase in the remaining life will not be likely by removing the defect. The following methods can be either implemented by divers or ROVs.
**Mechanical Clamp Repairs**

This is normally recommended for severely cracked components. In the case of tubular joints, the mechanical strengthening is achieved by the use of clamps formed from two or more slip-on collars held together by long threaded studs or hydraulic jacks. These will allow for either compression or tension loading to be applied. Accurate surveying of the damaged area is required as clamps rely on the friction between the tubular member and the collars to provide an extra load path. By installing two or more such collars on the members around a damaged site and interconnecting them with suitable structural members, a repair method can be implemented [1.66].

**Grouted Clamp Repairs**

Grouted clamp repairs are similar in principle to mechanical clamp repairs, in that they are placed around a joint to provide an alternative load path. They are, however, not tightly fitted around the damaged area. Grout is injected in the space between the clamps and the joint before the collars are stiffened, thus allowing for an evenly balanced fit around the member. The foremost disadvantage of this repair technique over the mechanical version is the restricted access to the outer surface of the original cracked area for future inspection and monitoring [1.66, 1.67].

**Internal Grouting of a Member**

Traditionally, grout injection was used to fill the voids within the pile guides once the fixed platform had been installed. It was later discovered that the introduction of grout into critical tubular nodes could yield considerable benefits. The chord is completely filled, through a small drilled hole or otherwise, with an unreinforced cementitious material. Care is taken as to ensure that the member under repair is completely filled with grout as any voids from incomplete filling may result in an area of weakness.

A fully grouted joint will become considerably stiffer resulting in reduced stress concentration values. The grout prevents significant chord ovalisation, which occurs in conventional tubular joints under either axial or out-of-plane bending loading.
modes. A reduction in the stress concentration factors, and hence the hot spot stress range, will enhance the fatigue life of a joint [1.66, 1.68]. The Health and Safety Executive document, OTH 92 368 [1.69] reports of a series of tests performed at the National Engineering Laboratory on conventional and grouted tubular welded T-joints. The main aim of the study was to compare the SCFs and the fatigue lives of the two sets of T-joints under the three modes of loading. A reduction in SCF of 40% and 30% was recorded for axially loaded joints and joints under out-of-plane bending, respectively. No significant reduction in stress was noticed for tubular members under in-plane bending.

**Electrochemical Machining**

Electrochemical Machining (ECM) is the controlled dissolution of the work piece material by contact with a strong chemical reagent in the presence of an electric current. The removal of the metal is controlled by anodic dissolution in an electrolytic cell in which the work piece is the anode and the tool the cathode. As a low voltage, direct current is passed through the cell, the electrolyte is pumped through the gap between the tool and the work piece. This results in metal dissolution. The tool is generally U-shaped and is made from an electrically conductive material such as copper or steel. The electrolyte is a salt solution, most commonly aqueous Sodium Chloride or aqueous Sodium Nitrate, which contains a large number of ions. ECM is generally used in industry to manufacture parts that are difficult to fabricate by mechanical machining. Its use is sporadic, as the high set-up cost makes it a less attractive option. It provides a good surface finish which is governed by the density of the current at the cutting face. As all removal repair techniques, ECM is generally used for cracks that have grown less than halfway through the thickness [1.70, 1.71].

**Grinding**

Weld toe grinding is a relatively inexpensive process widely used for both, improving the fatigue life of a joint by adding a radius to the weld toe to increase defect initiation life, or removing a surface crack. The grinding tool, which can be used manually or
via an automated process, is used to repair the defected area. The mechanical effects and heat induced during the process may lead to high residual stresses which can be detrimental to the fatigue life of the welded component. As such, grinding can be used in conjunction with peening to remove some of the residual stresses by applying compressive stresses to the surface of the repaired area. A second possible feature which might make grinding the less attractive option than other removal repairing techniques, such as ECM, is the fact that the process is not as controlled. The operator is more likely to cause surface damage or fail to completely remove the defect due to the lack of process controllability. Also, unlike ECM, the removed section cannot be analysed after grinding [1.67, 1.70].

**Welding**

Welding is regarded as one of the best repair procedures for offshore structures. It is often preceded by a crack removal method. The area removed is then filled by a chosen welding process, such as Manual Metal Arc (MMA) welding or Tungsten Inert Gas (TIG) welding. There are various techniques practised in the offshore industry, such as dry welding (at one atmosphere), wet welding, friction welding, and dry hyperbaric welding. The use of a hyperbaric chamber, though costly, allows for high quality welding in water depths of several hundred metres. The working habitat is assembled around the repair site and is dewatered by injecting gas of the pressure of the surrounding water [1.66].

Tests were performed by The Welding Institute on two fillet welded attachment specimens containing repaired partial-thickness and through-thickness fatigue cracks [1.72], in order to assess the fatigue performance of repaired welds. The partial-thickness crack was removed by burr grinding. Figure 1.25a illustrates the first specimen after crack removal. Magnetic particle inspection was used to ensure that the crack had been fully extracted. The repair was then completed by the MMA welding process.
Burr grinding was also used to remove all but the last 2 mm of the through-thickness crack. Figure 1.25b illustrates the second specimen after partial crack removal. The root of the defect was then rewelded by the TIG welding process and the filler runs were made by MMA welding. The two repaired joints were then tested as new specimens and the fatigue performance results were compared to undamaged specimens of similar geometry. In both cases, fatigue strengths comparable to that of undamaged joints were achieved.

**Crack Arrester Holes**

A traditional method of repairing or arresting through-thickness cracks is by drilling a hole at each end of the crack, thus removing the sharp crack tips. The resulting reduction in stress concentration and the subsequent increase in the remaining fatigue life are dependent on the radius of the holes and whether the crack tip has been fully removed. As for all removal techniques, it is recommended that MPI is performed on the repaired surface, to ensure that the entire crack (or crack tip) has been extracted [1.73, 1.66].

During the routine inspection of BP’s Magnus Platform in the North Sea, a flawed member at a depth of 182 m was discovered to be flooded. Inspection performed by divers revealed a through-thickness crack, approximately 500 mm in length, along the circumferential weld connecting the diagonal brace to a leg node stub. Crack arrester holes were introduced by trepanning to retard the crack growth. Trepanning has the advantage of retaining the core of the removed samples for further examination. This can be very useful in determining whether the crack tip has been removed. Crack arrester holes are a temporary measure, and thus a more permanent solution was still required. Abrasive water jetting was used to cut out part of the defective section, leaving a suitable opening for subsequent repairs to be implemented. Repair was then completed by TIG welding and MMA welding in a purpose-built hyperbaric chamber [1.74].
**Peening**

Peening is the process of impacting the surface of a work piece with hard objects, causing plastic deformation. The technique has a long history as hammer peening has been practised by metal workers and blacksmiths for centuries. It is now more common to use small, fast paced particles in order to obtain a better surface finish. The amount of deformation is governed by various factors such as the size of the particles, the time of exposure of the work piece, and the rate of impact of the shots.

Although strictly not a repair technique, it can be used with other methods such as grinding to induce surface improvement after removal. Controlled shot peening generates a defect tolerant surface by reducing notch and surface sensitivity and removing deleterious effects of abusive machining or heat treatment. This cold working, surface pre-stressing technique is used primarily to reduce component failures resulting from fatigue cracking. The residual stress is present due to deformation of the surface layer beyond its yield point. This, however, allows for sub-surface cracks to grow below the deformed surface. Inspection of such flaws is more difficult than surface-breaking flaws. Also, if the impact objects are not small enough in size, the surface finish will not be smooth, thus creating possible crack initiation sites [1.67, 1.71].

**1.11 SUMMARY AND SCOPE OF THESIS**

This thesis presents an investigation into the performance of tubular components, typically used in offshore structures, containing large defects. The significance of the residual strength of tubular members is highlighted.

This chapter has introduced various method used to evaluate the static capacity of intact tubular joints. These techniques are derived from large databases of tests on mainly small-scale joints. Furthermore, a simple procedure recommended by BS 7910 to conservatively estimate the residual strength of
cracked tubular joints has been reviewed. This technique, while relatively successful, has not been utilised to estimate the residual static strength of tubular joints manufactured from high strength steels. These types of steels, in welded form, may not perform as well as BS 7191 355D steel. Such information can be of great importance when considering deployment of flooded member detection for use with high strength steel tubular joints. The benefits and possible shortcomings of the use of high strength steels in the construction of offshore platforms have also been analysed.

The BS 7910 method for the prediction of the residual strength of tubular joints can only be used to allow comparison of the plastic collapse qualities. The Failure Assessment Diagram procedure introduced in Section 1.8 accounts for both, plastic collapse and fracture. The fracture axis of a FAD is dependent on knowledge of the stress intensity factor for a particular geometry. The limited SIF solutions for tubular joints, particularly those containing through-thickness cracks, have been discussed.

The final section is a review of the IRM procedures currently used by the offshore industry.

The following chapters are aimed at addressing the above issues.

Chapter 2 details an account of nine full-scale residual static strength tests on pre-cracked tubular joints manufactured from high strength steels. All joints contained at least one through-thickness defect. Two full-scale testing rigs, capable of applying a load equivalent to the plastic collapse capacity of uncracked specimens, were designed and constructed to test the six T-joints and the three Y-joints to failure. A novel digital photogrammetry method, which allows for three-dimensional displacement measurements during the tests, is introduced. The results of the tests are compared with data from previous studies and the BS 7910 method for estimating the residual strength of cracked tubular joints.
Four full-scale static strength tests on pre-cracked, circumferentially welded tubes manufactured from BS 7191 355D are reported in Chapter 3. Two specimens contained partial-thickness flaws, while the remaining two contained through-thickness flaws. Due to the successful implementation of the digital photogrammetry technique for the tubular joint tests, it was again employed to maximise the data capture during these tests. It is hoped that the correlation between the two different types of defects will provide a better understanding as to the effect of a deep partial-thickness flaw developing to a through-thickness flaw. Furthermore, it is anticipated that the data gathered from this and the previous sets of tests can be used to assess the conservatism or otherwise of relying on FMD for crack inspection of offshore tubular members.

A fracture mechanics study of tubular components containing large cracks is presented in Chapter 4. Several existing SIF solutions for tubular sections are reviewed. The most suitable technique for both, partial-thickness and through-thickness cracks were selected. The chosen technique was then validated by performing a finite element based study. A new SIF solution for tubular T-joints, containing through-thickness cracks, under axial loading is provided. The method is based on the SIF at the crack tip and the non-uniform stress distribution present in an axially loaded tubular T-joint. This information along with the results from Chapters 2 and 3 have contributed to the assessment of the tubular members using the FAD procedure.

Chapter 5 focuses on the effects of reduction in member strength on a structure. The local and the global responses of a structure to the presence of a large defect are reviewed. The importance of redundancy and multiple load paths are stressed and possible repair options are considered.
A summary of the conclusions derived from the findings of this investigation is presented in Chapter 6. Possible future work, aimed at improving the understanding of the behaviour of offshore structural components to cracking is recommended.
1.12 REFERENCES


[1.65] Pook, L. P., "Quality Technology", Course Notes, Department of Mechanical Engineering, University College London, 1996.


1.13 TABLES AND FIGURES

Table 1.1: Non-Dimensional Parameters of Tubular Joints

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Figure 1.4: Weld Toe Terminology
InCREASE IN STRESS DUE TO OVERALL JOINT GEOMETRY

STRESS IN BRACE WALL

STRESS IN BRACE

EXTRAPOLATION OF GEOMETRIC STRESS DISTRIBUTION TO WELD TOE

STRESS INCREASE DUE TO WELD GEOMETRY

BRACE "HOT-SPOT" STRESS = NOMINAL STRESS IN BRACE X SCFb

STRESS DISTRIBUTION IN BRACE

STRESS DISTRIBUTION IN CHORD

STRESS INCREASE DUE TO WELD GEOMETRY

CHRORD WALL

STRESS IN CHORD

EXTRAPOLATION OF GEOMETRIC STRESS DISTRIBUTION TO WELD TOE

STRESS INCREASE DUE TO OVERALL JOINT GEOMETRY

CHORD "HOT-SPOT" STRESS = NOMINAL STRESS IN BRACE X SCFb

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\[
d\alpha/dN = C(\Delta K)^m
\]

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\[ \frac{\delta_{\text{crit}}}{2 \pi \varepsilon_y a} \]

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Yield Strength

**Modern High Strength Steel**
\( \sigma_y = 450 \text{ MPa}, \frac{\sigma_y}{\sigma_{uts}} = 0.85 \)

**Conventional Structural Steel**
BS 4360 Gr50D / BS 7191 355D
\( \sigma_y = 350 \text{ MPa}, \frac{\sigma_y}{\sigma_{uts}} = 0.7 \)

**Figure 1.22:** Comparison of the Stress-Strain Characteristics for a High Strength Steel with a Medium Strength Steel

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CHAPTER 2

2.0 EXPERIMENTAL MEASUREMENT OF THE RESIDUAL STRENGTH OF CRACKED HIGH STRENGTH STEEL TUBULAR JOINTS

2.1 INTRODUCTION

In offshore structures, an extreme loading condition may occur at any time due to severe environmental conditions. Tubular joints in a structure may contain a relatively large crack at the time when the maximum design load is experienced. This study is directed towards developing a better understanding of the static strength of cracked tubular joints, in particular, those manufactured from high strength steels.

Most of the static strength tests on large-scale tubular joints of the type used in fixed platforms have failed in a ductile manner in the laboratory [2.1-2.4]. This is because the material has to pass an acceptance test based on the CTOD, which ensures ductile behaviour. The UK Health and Safety Executive (HSE) have used this information as the basis for guidance for medium strength structural steels [2.5-2.7].

Recently, high strength steels have been used in the construction of fixed platforms and for Jack-ups used for production rather than exploration. In addition, the level of in-service inspection is reducing as more emphasis is placed on the use of flooded member detection to indicate through-thickness cracking. This has resulted in the increased reliance of the designers on the residual capacity of a cracked component.

Very little information is available on the residual strength of cracked tubular joints and even less for those constructed from high strength steels. These types of steels
have a high yield to ultimate ratio and could, under certain circumstances, fail in a brittle manner. A typical high strength steel (SE 702), in welded form, may not be as ductile as BS 7191 355D / BS 4360 50D, which is a steel often used in the construction of offshore jacket structures. Furthermore, the BS 7910 [2.8] procedure for estimating the residual strength of cracked tubular joints has not been verified for use with tubular joints manufactured from high strength steels. This procedure can be of great benefit to the designers and inspectors. For these reasons, it was decided to conduct full-scale static strength tests on tubular joints manufactured from SE 702.

The availability of pre-cracked joints from a previous study [2.9] provided an opportunity for data comparison with joints made from conventional structural steels such as BS 4360 50D / BS 7191 355D. The details of the experimental residual strength test programme involving nine full-scale welded tubular joints are presented in this chapter.

2.2 EXPERIMENTAL DETAILS

2.2.1 Specimens

A total of nine cracked tubular joints were available from previous projects.

Three Y-joints were part of a Variable Amplitude Corrosion Fatigue study. The project involved out-of-plane bending fatigue loading on Y-joints manufactured from SE 702 in two different testing environments. The main objective was to examine the corrosion fatigue behaviour of the joints under simulated service loading and environmental conditions. All joints were fatigue loaded until a through-thickness crack was detected [2.9, 2.10].

Six T-joints were also part of the full-scale fatigue-testing programme. These axially loaded joints were also tested under two different environmental conditions.
The effects of cathodic protection on the corrosion fatigue behaviour of these joints under constant amplitude loading was analysed. All T-joints were cracked on at least one side [2.9, 2.11].

The original numbering sequence of the joints, used in the two previous studies, was retained for consistency.

The joint parameters are shown in Table 2.1, while the non-dimensional parameters are listed in Table 2.2. Figure 2.1 shows the dimensions for each type of tubular joint. Table 2.3 shows the testing history of the nine joints. Figures 2.2 and 2.3 show the percentage cracked area and the position of each crack for the Y-joints and the T-joints, respectively.

### 2.2.2 Materials

All nine tubular joints were fabricated from SE 702 which is a member of the Superelso family of steels. It is Creusot Loire Industrie's (CLi) equivalent of the A517 GrQ standard [2.12]. The quoted chemical composition is given in Table 2.4.

The specified mechanical properties are given in Table 2.5 [2.13].

Independent tensile tests on 5.5 mm diameter specimens were carried out at Cranfield University on the same batch of SE 702 used to fabricate the tubular joints. There were no significant differences between the obtained and the quoted values. Data from the Charpy impact tests performed at Cranfield are presented in Table 2.6 [2.11]

Both CLi and Cranfield University also conducted hardness tests on the parent plate, the heat affected zone, and the weld metal of T-Butt weld specimens. The data produced are presented as Vickers Hardness numbers in Tables 2.7 and 2.8 [2.11].
2.2.3 Joint Fabrication

All joints were manufactured by CLi. Both the brace and the chord were rolled in two halves and subsequently seam welded together to produce tubes of 16 mm wall thickness. Details of the seam welding is given in Table 2.9. Post weld heat treatment was applied to the seam welds [2.9]. The position of the seam welds is illustrated in Figure 2.4.

It was important that the welding between the brace and the chord was representative of that used in the construction of offshore structures as the weld detail is known to have a significant effect on the fatigue life of the joint. Details of the weld at the intersection are given in Table 2.10. It should also be noted that no post weld heat treatment was specified for the completed joint [2.9].

2.2.4 Test Arrangement

Full-scale tubular joint tests are expensive and difficult to perform. A great amount of time and effort was involved to develop two test set-ups capable of performing these tests. The first step in designing such a testing rig is to ensure that it can safely provide the required loading. It was thus important to estimate the loading capacity needed for each type of specimen. It was decided that each rig should be capable of applying a load equivalent to the capacity required to plastically collapse an uncracked specimen. These values were calculated using the four sets of equations mentioned in Section 1.6 and are listed in Table 2.11. The two most commonly used and updated sets, the design equations from the withdrawn HSE Guidance Notes [2.14] and the API RP2A Load and Resistance Factor Design (LRFD) equations [2.15], were utilised for further analysis. The LRFD equations were used rather than the API RP2A Working Stress Design (WSD) equations [2.16]. The latter is concerned with loading on the entire structure while the former relates to the adequacy of a component to withstand its intended load and was thus considered to be more suitable for this type of component testing.
2.2.4.1 Y-joints

The Y-joints were tested in a purpose built rig, which consisted of a 250 kN Instron actuator with a 100 mm stroke length, capable of applying out-of-plane bending loading to the brace of a Y or a K-joint. The system was operated from an Instron mini-controller. The two ends of the chord were restrained from movement in all directions. This set-up was also used for fatigue pre-cracking of the first Y-joint specimen, Y1. The failure loads predicted from the API RP2A LRFD and the HSE Guidance Notes for an uncracked Y-joint were approximately 413 kN and 426 kN, respectively. Therefore, there was a need for an actuator with at least as much capacity as the larger of the two calculated values. After the initial fatigue pre-cracking (described in 2.2.6), the 250 kN actuator was replaced with a 500 kN actuator with a similar stroke length. The new set-up is shown in Figure 2.5. This involved modifying the rig as the total length of the two actuators was not identical. The resulting format did not alter the angle or the direction of the applied load.

2.2.4.2 T-joints

Testing rigs with axial loading capacities of up to 2.5 MN are available at University College London. The use of the 2.5 MN test machine would not have been easy, as the end fittings were not readily available. Also, the fact that 2.5 MN was only about 70% of the estimated static strength of the uncracked joint led to consideration of alternative means to load the joint. It was decided that the rig which was employed to grow the fatigue cracks during the Constant Amplitude Corrosion Fatigue project [2.9] would be the best option available. A 1 MN servo-hydraulic Instron actuator with a stroke length of 100 mm had been used to apply the fatigue cycles. The actuator was operated from an Instron mini-controller. A schematic of this rig is illustrated in Figure 2.6. This set-up was used to grow cracks in the first T-joint (T6). As the uncracked failure load was predicted at about 3.64 MN, a larger actuator and a major change in the layout of the rig was required.

A system of two jacks pushing against a box section was designed. The set-up is illustrated in Figure 2.7. The box section consisted of four smaller rectangular
sections welded together and reinforced by additional welding of steel plates, as shown in Figure 2.8. High strength steel studdings, which were passed through the box, allowed the box to be bolted to the backing ring around the brace. Before each test, the backing ring was placed around the brace, while another steel ring was welded to the outer surface of the brace. As the jacks exerted force on the box, the backing ring came in contact with, and subsequently applied load on, the welded ring. Safety calculations were carried out to ensure that the welded ring would be able to withstand the maximum predicted load. The jacks were powered via a single hydraulic pump which could reach an oil pressure of up to 700 bar. This corresponded to a capacity of about 2500 kN per jack. A pressure gauge allowed the total system pressure to be monitored.

As an extra safety precaution, 2000 lbs of sandbags were placed to the rear of the reinforced box, to avoid the sudden displacement of the ‘brace-studding-box’ assembly after final fracture.

2.2.5 Instrumentation

As destructive tests are costly to perform, every effort was made to maximise the data collected. The following section depicts the various instrumentation techniques utilised.

2.2.5.1 SCXI Data Acquisition System

An SCXI (Signal Conditioning Extensions for Installations) data acquisition system made by National Instruments was utilised to record measurements from strain gauges, Linear Variable Displacement Transducers (LVDTs) and other voltage outputting devices. It consisted of two SCXI-1322 boards used for measuring strain and an SCXI-1100 board for monitoring changes in potential difference. The strain measuring boards were connected to 32 external terminals, each capable of recording from one linear gauge. A panel comprising of 32 BNC sockets was the link between the voltage outputting devices and the SCXI-1100 board.
In order to be able to use the hardware, a program had to be written and tested. The programming language selected was ‘C’ and the entire software was programmed in Lab Windows/CVI (‘C’ for Virtual Instrumentation) environment. The program was executed during each test by using a Panasonic laptop computer designed for use in experimental conditions. A shielded parallel cable was used to connect the computer to the data acquisition system. A single channel display was a feature of the user interface during testing.

All the measurements were output at the end of each test to a single data file. The separation and manipulation of the displacement and strain measurements were accomplished by transferring the data to a spreadsheet.

The selection of the recording rate was an important factor in achieving accurate results. The speed at which the data could be collected was variable from 1 to 100 channels per second. Tests were performed on the first T-joint (T6) by connecting the system to two strain gauges placed on the brace and applying constant low amplitude cyclic load. As the rate was set at a value above 20 channels per second, fluctuations, most probably background noise, could be observed on the strain-time history plot. It was, however, important for the rate to be reasonably high as to not omit any sudden changes during the test. The final value selected was 20 channels per second.

It should be noted that the first SCXI-1322 board was not used for any of the tests, thus limiting the use of the strain gauges to the 16 channels available from ‘board 2’. The fault was detected while performance tests were being carried out on a separate tubular joint set-up. Both boards were connected to strain gauges on the brace of a full-scale cruciform tubular joint. Low constant amplitude cyclic loads were applied. The test was performed to confirm the speed of data acquisition at 20 channels per second and to verify that both boards functioned adequately. From the recorded results it could be seen that most of the channels connected to ‘board 1’ showed large fluctuations in the strain readings while the rest were constant at zero.
The same strain gauge channels were then connected to ‘board 2’ and the results were a steady cyclic strain-time plot. This indicated an obvious fault with ‘board 1’. All attempts at repairing ‘board 1’ failed and consequently it was not used for any of the tests. Figure 2.9 illustrates the SCXI system set-up before a test.

2.2.5.2 Strain Gauges

Stacked Rosette strain gauges were bonded to the surface of the joint around the crack area and on the brace. For the first two tests involving the Y-joints (Y1 and Y4), gauges were placed 50 mm away from the crack tip. Two more gauges were bonded to the brace in line with the load application point and the saddle, parallel to the seam weld, as shown in Figure 2.10. The same approach was employed for the T-joint tests concerning the gauges around the crack area. The gauges on the brace were placed at 180° to each other for the first two T-joint tests (T6 and T1) and from then on at right angles to each other so that any conceivable bending in the system could be detected and possibly quantified. The gauges were then connected to the SCXI-1322 board via a series of terminals and electric cables.

Initialisation process was performed for all channels via the user interface to ensure every gauge had an initial value of zero. Table 2.12 shows the specification of the strain gauges.

2.2.5.3 Linear Variable Displacement Transducers

Two types of LVDTs were used. The transducer with the larger stroke length (DC25) was used to measure the brace displacement in the direction of the applied load. The smaller LVDTs were used for both chord ovalisation measurement and the wedge gauge assembly.

As the brace is axially loaded, load is transferred through the weld and to the chord. The shape of the cross-section of the chord thus changes from circular to elliptical, away from the clamped ends. The value of this deformation was recorded due to the fact that the chord deformation can affect the crack opening displacement (COD)
readings. The ovalisation of the chord for the T-joints was monitored by the DC15 LVDT, which had a stroke length of 30 mm, mounted on a purpose built stand. The values were recorded by the SCXI data acquisition system at regular time intervals. Figure 2.11 shows the set-up for the chord ovalisation measurement. Due to the lack of access to the inside of the chord, ovalisation measurements were not recorded for the Y-joints.

A mechanism of springs and LVDTs was designed to measure crack opening displacement. The set-up is shown in Figure 2.12. A hole was drilled through the crack, approximately 50 mm from either crack tip. Tapered steel components with circular cross-sections were placed at the outer mouth of the holes. The DC15 LVDTs were joined to these sections from the inside of the chord via springs. As the crack opened, the tapered sections would enter the hole further and thus a displacement could be recorded by the LVDT. The technique was employed for the initial two attempts at T6 and the testing of Y1 and proved successful at low amplitude loads. It was later discovered that as the load applied was increased, a large amount of mode III (tearing mode) hindered the tapered component from directly entering the drilled hole, and thus rendered the wedge gauge results inadequate in terms of measuring crack opening displacement.

Both types of LVDTs were linked to the SCXI-1100 board via BNC connectors.

The LVDTs could either be calibrated online using the SCXI data acquisition system or, alternatively, a previously recorded constant could be input via the user interface. A constant excitation voltage was supplied by a 10 V d.c. power supply throughout the tests. Table 2.13 shows the parameters of both types of LVDTs.

2.2.5.4 Load Cell
A load cell was used to provide confirmation of the load output from the jacks. It was situated to the rear of one of the jacks. An excitation voltage was supplied through the same power supply used for the LVDTs. The readings from the load cell
were initially recorded via the SCXI unit. It was found that the voltage measurement feature of the SCXI unit could not quantify the readings to the accuracy required. A digital voltmeter capable of recording potential difference to 10 μV increments was used instead.

2.2.5.5 **Digital Photogrammetry**

Digital photogrammetry is a non-contacting, optical measurement technique which uses high resolution, high quality imaging. It is used to complement conventional instrumentation in structural testing and monitoring. In its most basic form, the technique is based on following the movements of points on a specimen using a number of cameras and a computer. It is then possible to acquire sequences of images from which changes in the three-dimensional shape of a structure under test can be ascertained. Rigorous least squares based analysis is used in the numerical processing associated with the technique so that the three-dimensional co-ordinates are accompanied by statistical data indicating their accuracy, precision, and reliability.

The digital photogrammetric technique is extremely flexible, able to acquire measurement data simultaneously over the surfaces of large objects, and has been proven through its successful application in monitoring spatial deformation of a wide range of structures [2.17, 2.18]. However, this is the first time it has been used in conjunction with tubular joint experiments. With the help of the Department of Geomatic Engineering at University College London, this technique was employed to complement conventional instrumentation on all T-joint experiments.

The technique proved the most effective and accurate available means for measuring crack opening displacements of the magnitude and range required for this project. Its major advantages include the fact that it is a non-contacting measuring system and is very accurate, as it has been known to measure with sub-millimetre accuracy depending on the number of cameras being used [2.17].
In order to obtain maximum benefit from the technique a suitable three-dimensional axis co-ordinate system was defined so that the data recorded could be interpreted in Cartesian co-ordinates. For ease of camera calibration and presentation of the data in the required form, the brace of each tubular joint was aligned with the y-axis, while the x-axis was positioned along the chord and the z-axis perpendicular to both the chord and brace, as illustrated in Figure 2.13.

Retro-reflective targets with an adhesive side were placed on the specimens in a relatively accurate grid. A denser cluster of targets was placed on the area around the weld toe. Figure 2.13 shows the arrangements of the targets on a T-joint. As an independent reference, larger targets were placed in the field of view of the cameras, at locations where they were deemed to be completely stationary and not effected by the movements during the test. The distances between several of the targets were measured manually for scaling purposes and to provide an independent check on the three-dimensional target co-ordinates computed with the technique. A simple illustration of the system is shown in Figure 2.14.

2.2.6 Fatigue Pre-Cracking

The original rigs with modifications, which were previously used to grow the fatigue cracks in all nine joints, were used to grow the cracks in two of the specimens (Y1 and T6). It was then decided that the remaining Y and T-joints covered a reasonable range of percentage cracked areas (16%-72%) and thus pre-cracking for the rest of the specimens was not required. Table 2.14 shows the original crack sizes and the pre-cracking load range applied to Y1 and T6, along with the final crack sizes.

2.2.7 Elastic Loading

Elastic loading was carried out prior to all the Y-joint tests and the first T-joint test, in order to check the operation of the various gauges and the outputs from the actuator. Each specimen was loaded to approximately 5% of the total capacity and then unloaded to ensure that the instrumentation readings returned to zero.
2.2.8 Static Strength Test

**Y-Joints**
A PSI function generator was used to generate a slow ramp from zero up to the maximum capacity of the actuator. Load versus displacement data were obtained via the LVDT and the load cell of the actuator. Both readings were recorded at 10-second intervals using two digital voltmeters connected to the mini-controller.

It was known that the 100 mm stroke length was not enough for this test. To overcome this, the stroke was fully extended, the base of the actuator was packed with spacers while the stroke was reduced. The test was then restarted.

The test was stopped when either, the crack reached the crown positions on the joints, or when the rate of brace displacement increased rapidly relative to a small increase in load.

**T-Joints**
The DC25 LVDT, which had a stroke of 50 mm, was calibrated and attached to the back of the box section, in line with the centre-line of the brace. The SCXI data acquisition system was used to obtain the displacement readings as a function of time during the tests.

The oil pressure regulator of the hydraulic pump was used as the manual load controller for the duration of the tests.

For the first two tests, the load was measured using the strain gauges attached to the brace. For tests T4, T2, T5 and T3 the load cell was used. In all tests, the jack pressure was monitored to give confirmation of the load readings. The values from the load cell were consistently about 8% lower than those of the pressure gauge. This may be attributed to the fact that there is a certain amount of friction loss in the jacking system which could not be accurately quantified.
Sequences of photogrammetry images were captured by dimming the surrounding lights and shining a high intensity white light onto the targets, which subsequently reflect the light back. The image sequences were recorded by two synchronised digital cameras at a previously specified rate and were transferred to the computer. These images were then combined so that they could be displayed as an animation of the test in 2-D or 3-D. The data from each target also provided displacement values in all three co-ordinates as well as a combined value for movement in three-dimensions. Figure 2.15 shows the difference between two images from the initial and the latter stages of a test [2.19, 2.20], while Figures 2.16a and 2.16b show the image of the same T-joint captured from both cameras at the same instant.

Figure 2.17 demonstrates one of the features of this technique. It shows the three-dimensional path of the targets during one of the tests [2.21].

The procurement of the images from photogrammetry also allowed for the measurement of the crack length. Each image was viewed carefully and a decision was made as to how far the crack has progressed using the retro-reflective targets around the crack as reference points. Although this method of measurement was not very accurate, the information obtained has been valuable and necessary.

Tables 2.15 (i-vi) show the crack growth values at various time increments and load values during each T-joint test.

Tearing of the crack could be heard throughout all of the tests.

The tests were deemed complete when total separation between the brace and the chord was observed. Alternatively, for two of the tests (T4 and T2), one side of the joint failed completely, which resulted in a substantial drop in load being taken by the joint. There was normally a prelude in that the pressure reading from the gauge would drop off by a few percent before failure occurred. Figure 2.18 shows the separated brace from the chord for test T1.
A video account of the last three tests involving a T-joint (T2, T5 and T3) was recorded. The joints were positioned so that the crack (or the larger of the two cracks) could be captured in the camera shot. Crack propagation could be observed, especially during the latter stages of each test. The tests were recorded on 8 mm video-camera tapes and were later digitised and recorded on compact disc. These are included in Appendix B.

2.3 RESULTS AND DISCUSSION

The results of both sets of tests will be discussed in this section. There were more data collected for the T-joints than the tests involving the Y-joints. Load-displacement graphs as well as the photogrammetry based data, such as the crack opening displacement, are analysed for all the specimens. The information obtained is correlated to obtain a better understanding as to what transpired during each test on a macroscopic level.

Certain measurements recorded were deemed unreliable and will not be discussed after this paragraph. There was a problem encountered during the third test (T4) which could not be rectified. Therefore, only a load-displacement graph will be presented for this test. A second source of discrepancy could have been the strain gauge results. While the problem regarding the SCXI-1322 boards was overcome (Section 2.2.5.1), the strain gauge readings from those placed around the weld toe near the cracked region were judged to be erroneous. The strain around the highly stressed areas was too great and the gauges subsequently became detached in the latter stages of the first few tests. Therefore, only the gauges on the brace of each specimen were used.
The main test results are summarised in Table 2.16 which shows the initial tearing load and the peak load achieved, and the original percentage cracked area for each specimen.

The tests will be discussed in the order which they were tested, starting with the Y-joint tests.

### 2.3.1 Y-Joints

#### 2.3.1.1 Specimen Y1

As explained in Section 2.2.8, all Y-joint specimens had to be tested in several stages. The percentage cracked area of specimen Y1 had been increased from 45% to about 70% by pre-cracking. The next stage of the test involved three large cycles. Figure 2.19 illustrates the load-displacement curves for the static strength testing of the first Y-joint. The first cycle shows that the load is linearly related to the displacement up to a load of 110 kN. Subsequently, the slope of the load-displacement curve changes to yield an approximately linear region up to about 160 kN. After this point, the crack began to tear at a rapid rate until the maximum stroke length had been reached. The joint had to then be unloaded. Spacers were added to the base of the actuator to allow an increase in the overall brace displacement.

The application of a second load cycle resulted in a linear slope, until the last two points, where more tearing caused a rapid increase in displacement.

The same process was undertaken for the third and final cycle. The results are similar to the second cycle, as only the last three points are not in line with the previous loading curve. It should be noted that crack extension occurred in each cycle. By the last run, the cracked area was almost 100% (half the total welded area) and initial branching of the crack into the chord was observed.

The specimen was loaded one last time. On this occasion, however, the maximum load taken was about 20 kN less than the previous cycles. The results from this
cycle are omitted as they provide no useful data. This type of 'last run' was performed for all the other Y-joint specimens in order to ensure that the joint had failed.

2.3.1.2 Specimen Y4
Specimen Y4 was tested in the same manner as the previous Y-joint. Figure 2.20 illustrates the two cycles performed on this specimen. The first two cycles were enough for the joint to reach its peak capacity of 270 kN. As before, both curves are linear, up until the last few points. No spacers were required for the first two cycles. Spacers were added for the confirmation cycle, during which the load reached approximately 250 kN. The crack had started to branch off into the chord indicating that the test was over.

The load-displacement curve for this specimen indicates a much less ductile fracture than the previous test. This could possibly be explained by referring to the cathodic protection potential used during the previous year's fatigue testing of Y4. Table 2.3 indicates this value to be -1000 mV Ag/AgCl. One of the major finding of reference 2.10 was the fact that the reduction of the voltage potential from -800 mV (Y3) to -1000 mV (Y4) could lower the total fatigue life of the specimens (manufactured from SE 702) by about 30%. This is likely due to the presence of hydrogen in the sea water environment. It should be noted that there was no evidence to suggest that SE 702 was any more susceptible to hydrogen embrittlement than other high strength steels of similar grades.

2.3.1.3 Specimen Y3
Test Y3 was expected to be very similar to Y4 as the percentage cracked areas were almost identical.

Figure 2.21 displays the three cycles that were performed on Y3. The first curve is not completely linear after the early part of the test. The displacement is increasing more rapidly with respect to the load applied. A new cycle was applied, after the
addition of spacers to the base of the actuator, as the stroke had run out on the previous test.

The second loading cycle is linear until the last two points which are in line with the final few points of the previous run. The result of the final cycle is very similar to the second run. The overall brace displacement was approximately 190 mm. A complete load-displacement graph can be plotted by joining the last few points of the second and third test to the final stages of the first test.

### 2.3.2 T-Joints

#### 2.3.2.1 Specimen T6

The test was attempted three times, twice with the 1000 kN servo-hydraulic actuator in the original T-joint rig used to pre-crack the specimen, and once with the static jacks and box beam set-up. The first servo-hydraulic actuator test reached a maximum load of 930 kN. The second servo-hydraulic actuator test (using a higher pressure) reached a maximum load of 950 kN with clearer evidence of reaching a peak value. The third test (using the jacks) resulted in total separation of the brace from the chord at a peak load of approximately 960 kN.

The brace displacements for the first two tests were recorded from the LVDT of the servo-hydraulic actuator, whilst the LVDT at the back of the box beam was used for the third test. The displacement measured in the third test is likely to be more representative of the brace displacement.

Figure 2.22 shows the load-displacement curves for T6 using the two methods of brace displacement, the chord ovalisation, and the maximum crack opening displacement data. It can be seen that the chord ovality curve tends to a constant value of displacement in the latter stages of the test. The general shape of the other three curves was similar in that all three had a distinctive peak at the maximum load. From the similarity of the load-Maximum COD curve to the load-brace displacement curve, it may be assumed that the top crack was the dominant defect.
The fracture surface indicated ductile tearing throughout, excluding a small region at the crown points of the chord.

Figure 2.23 is a plot of mode I crack opening displacement along the crack at various loads throughout the test. The curves show that the crack was relatively symmetrical about the hot spot stress point.

The change in crack length as a function of load was measured and recorded in Table 2.15(i).

2.3.2.2 Specimen T1

Figure 2.24 shows the plot of load versus brace displacement, chord ovality, and maximum COD values. The plot has a distinctive peak at approximately 3000 kN. As the maximum load was reached, continuous tearing occurred and the pressure gauge indicated a drop in load (displayed by the unloading section). Figure 2.18 shows the separated brace from the chord for test T1.

The fracture surface showed a large amount of ductile tearing at the top. The bottom surface was mostly light reflective and crystallographic, implying brittle fracture.

Figure 2.25 shows part of the fracture surface of the second test. The original fatigue crack can be clearly seen.

Figure 2.26 illustrates the COD readings. The crack growth is approximately equal from both sides around the saddle and it has a symmetrical shape. This figure also shows the rapid tearing stage as the end of the test took place. The change in crack length as a function of load was measured and recorded in Table 2.15(ii).
2.3.2.3 Specimen T4

The load-displacement plot results are shown in Figure 2.27. Some data were lost for this test.

From Figure 2.27, an elastic region can be observed, followed by a peak at about 3050 kN.

The change in crack length as a function of load was measured and recorded in Table 2.15(iii).

2.3.2.4 Specimen T2

Specimen T2 was the only specimen that had equal size cracks on both sides (approximately 17%).

Figure 2.28 is the load-displacement plot using various methods of measuring displacement. It has a clear and distinctive peak at about 2800 kN. The considerably smaller value of maximum COD compared with the other displacements could be indicative of the fact that the monitored crack (at the top) was not the dominant defect. The test ended as there was a sudden drop in the load. About 20% of the brace remained attached to the chord.

There was comparatively little tearing during this test, as indicated by the fracture surface. There was more tearing at the bottom which meant that the bottom crack had become the dominant defect. The fracture surface indicated more brittle fracture during this test than any of the others.

Figure 2.29 illustrates the COD values at different loads during the test. The crack did not extensively increase in length.

The change in crack length as a function of load was measured and recorded in Table 2.15(iv).
2.3.2.5 *Specimen T5*

The load-displacement curves are shown in Figure 2.30. It can be seen that there was a plateau once the maximum force was reached. From 20 mm until about 42 mm the crack was tearing under constant load. All four curves closely resembled each other. It should be noted that the chord was permanently deformed after the test.

The fracture surface was mostly dull and fibrous around the original crack, thus suggesting ductile tearing. There was more evidence of brittle fracture on the opposite side of the weld.

From Figure 2.31 it can be seen that the crack grew symmetrically and at a relatively constant rate. The crack opening displacement for this test is the largest of the whole project. This can partially be explained by the large deformation in the chord indicated by the chord ovalisation readings.

The change in crack length as a function of load was measured and recorded in Table 2.15(v).

2.3.2.5 *Specimen T3*

The load-displacement curves are shown in Figure 2.32. The curve shows a plateau after reaching the maximum load of 3100 kN. It can be argued that the onset of unstable tearing can be assessed by observing the gradient of the load-maximum COD curve. This information can be useful for monitoring purposes. Visible deformation was observed in testing of this specimen, although not to the extent of the previous test.

The fracture surfaces indicated a large amount of ductile tearing on the top side and to a lesser extent on the bottom face.
The crack opening displacement curves (Figure 2.33) indicate that the crack did not grow evenly.

The change in crack length as a function of load was measured and recorded in Table 2.15(vi).

2.3.3 Analysis of Data

The peak load in each case was used with the HSE limit state characteristic loads to give the failure load ratio and plotted as a function of crack size. The data are shown in Figure 2.34 together with the mean line obtained from Figure 4, Reference 2.22. This line includes data from large-scale tests, small-scale tests and numerical analysis. The implications of including data from various methods were discussed in Chapter 1. Also included is the BS 7910 [2.8] procedure for the prediction of the static strength of cracked tubular joints, using the area reduction factor introduced in Section 1.7.

There is an obvious linear relationship between the static strength and the crack size. In most cases, the failure data for SE 702 are below the mean line from Figure 4, Reference 2.22. A mean line for the high strength steel data was plotted to further establish the relationship between the two sets of data. There is only a small difference between the mean lines, with the high strength steel mean line being about 7% lower in relation to the intercept at the ordinate.

It should be noted that the HSE characteristic load for high strength steels is higher than that of common structural steels due to the higher yield strengths. This indicates that, for the same load ratio, the tubular joint manufactured from the higher strength steel was capable of withstanding a higher maximum load. This implies that tested tubular joints performed well when considering their plastic collapse behaviour.
The results from this study have been included in the HSE document *Offshore Technology Report 2001/080* [2.23]. Figure 2.35 is taken from this document. Unfortunately, only seven of the nine tests have been included. Also, there is a clear discrepancy in calculating the ratio of the experimental maximum load to HSE characteristic load. For example, the final two Y-joint tests are represented by the two squares about the 45% cracked area region. The maximum load achieved by both specimens was approximately 270 kN. The HSE characteristic load for out-of-plane bending load applied to a Y-joint was calculated as 426 kN. Therefore, the load ratio should approximately be 0.63, as presented in Figure 2.34 and NOT as a value less than 0.5, as represented in Figure 2.35. It is likely that there was an error in calculating the uncracked strength of the joints.

The normalised static strength capacity approach appears to be simple to operate and provides an appropriate method for the offshore industry. However, it does not account for the possible effects of extension of the initial crack by fracture. It only provides a useful guidance on plastic collapse. The Failure Assessment Diagram procedure outlined in Chapter 1, accounts for both, fracture and plastic collapse. A Failure Assessment Diagram analysis was performed for all T-joint specimens and is reported in Chapter 4.

### 2.4 CONCLUSIONS

Nine static strength tests were successfully completed on cracked high strength steel tubular welded joints (T and Y) made from SE 702. Two full-scale testing rigs, capable of applying a load equivalent to the plastic collapse capacity of uncracked specimens, were designed and constructed. The specimens all failed in a ductile manner.

The capacity of the high strength steel tubular joints was not severely hampered by relatively large cracked sections. Indeed, Specimen T2 (35.4% cracked area)
sustained an initial tearing load of about 2296 kN, i.e. 92\% of the estimated capacity. While these results can be used to partially justify the use of FMD on structures with a relatively high degree of redundancy, caution should be adhered. Results from tests Y4 indicate that a specimen fatigue tested under a relatively high CP potential is more likely to be susceptible to brittle fracture. It would therefore be essential to use voltage limiting diodes to restrict the voltage potential and thus reduce the risk of brittle fracture for a component manufactured from high strength steels.

Normalised ultimate static strength results were compared with typical data produced from various tests. It was found that the mean reduction in the static strength of cracked high strength steel joints relative to the uncracked strength was lower than that of the available data by 7\% at the ordinate intercept. The mean line for the high strength steel data was, however, greater than the recommended BS 7910 procedure for estimating the residual strength. These results indicate that tubular joints manufactured from high strength steels do not suffer from a significant lower cracked capacity. This, however, only allows for the comparison of plastic collapse qualities of the specimens. There is a need for Failure Assessment Diagram analysis which accounts for both plastic collapse and fracture. The data obtained during this study will contribute to calculating the FAD parameters.

A novel digital photogrammetric method has been introduced. The technique allows for three-dimensional measurements in real time of the deformation in the vicinity of the brace-chord intersection.
2.5 REFERENCES


### 2.6 TABLES AND FIGURES

Table 2.1: Parameters of the Specimens

<table>
<thead>
<tr>
<th>Tubular Joint Parameters</th>
<th>Notation</th>
<th>Y-Joint</th>
<th>T-Joint</th>
</tr>
</thead>
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<tr>
<td>Chord Thickness</td>
<td>T</td>
<td>16 mm</td>
<td>16 mm</td>
</tr>
<tr>
<td>Chord Diameter</td>
<td>D</td>
<td>457 mm</td>
<td>457 mm</td>
</tr>
<tr>
<td>Chord Length</td>
<td>L</td>
<td>2480 mm</td>
<td>1660 mm</td>
</tr>
<tr>
<td>Brace Thickness</td>
<td>t</td>
<td>16 mm</td>
<td>16 mm</td>
</tr>
<tr>
<td>Brace Diameter</td>
<td>d</td>
<td>324 mm</td>
<td>324 mm</td>
</tr>
<tr>
<td>Brace Length</td>
<td>I</td>
<td>1390 mm</td>
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<tr>
<td>Brace Angle</td>
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<td>90°</td>
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Table 2.2: Non-Dimensional Parameters of the Specimens

<table>
<thead>
<tr>
<th>Non-Dimensional Parameters</th>
<th>Notation</th>
<th>Y-Joint</th>
<th>T-Joint</th>
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<tr>
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</tr>
<tr>
<td>d/D</td>
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<td>0.71</td>
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<tr>
<td>D/2T</td>
<td>γ</td>
<td>14.28</td>
<td>14.28</td>
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<tr>
<td>t/T</td>
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Table 2.3: Testing History of the Specimens

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<th>Test in order tested</th>
<th>Tested Environment</th>
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<tr>
<td>Y1</td>
<td>Air</td>
<td>-</td>
<td>45.0%</td>
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<tr>
<td>Y3</td>
<td>Sea Water</td>
<td>-800 mV Ag/AgCl</td>
<td>45.6%</td>
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<td>Y4</td>
<td>Sea Water</td>
<td>-1000 mV Ag/AgCl</td>
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<tr>
<td>T1</td>
<td>Air</td>
<td>-</td>
<td>16.3%</td>
</tr>
<tr>
<td>T2</td>
<td>Air</td>
<td>-</td>
<td>18.2%</td>
</tr>
<tr>
<td>T3</td>
<td>Sea Water</td>
<td>-1000 mV Ag/AgCl</td>
<td>16.3%</td>
</tr>
<tr>
<td>T4</td>
<td>Sea Water</td>
<td>-800 mV Ag/AgCl</td>
<td>7.8%</td>
</tr>
<tr>
<td>T5</td>
<td>Sea Water</td>
<td>-1000 mV Ag/AgCl</td>
<td>18.0%</td>
</tr>
<tr>
<td>T6</td>
<td>Sea Water</td>
<td>-800 mV Ag/AgCl</td>
<td>18.0%</td>
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Table 2.4: Chemical Composition of SE 702

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<th>Element</th>
<th>SE 702 Quoted Chemical Composition (%)</th>
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<td>C</td>
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<td>Si</td>
<td>0.25</td>
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<td>S</td>
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</tr>
<tr>
<td>P</td>
<td>0.009</td>
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<td>Ni</td>
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<td>Cr</td>
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<tr>
<td>Mo</td>
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<tr>
<td>B</td>
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<td>V</td>
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<td>Cu</td>
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<td>Sn</td>
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<tr>
<td>Co</td>
<td>0.01</td>
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<tr>
<td>Nb</td>
<td>&lt;0.01</td>
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<td>As</td>
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Table 2.5: Mechanical Properties of SE 702

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<th>$\sigma_y$ (MPa)</th>
<th>UTS (MPa)</th>
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<th>Carbon Eqv. (IIW)</th>
<th>Paris Constants 'C' and 'm'</th>
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<tr>
<td>700</td>
<td>790-940</td>
<td>16</td>
<td>0.599</td>
<td>'2.72 x 10^{-12}', and '3.532'</td>
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Table 2.6: SE 702 Charpy Test Data

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<th>Specimen</th>
<th>Charpy Energy (J)</th>
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<td>Room Temperature</td>
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</tr>
<tr>
<td></td>
<td>S2</td>
<td>152</td>
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<td>-40</td>
<td>S3</td>
<td>127</td>
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<td>-60</td>
<td>S4</td>
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<td>-60</td>
<td>S5</td>
<td>92</td>
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Table 2.7: SE 702 Vickers Hardness Data

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<td>HAZ</td>
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<td>Weld Metal</td>
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Table 2.8: SE 702 Hardness Data

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<td>249</td>
<td>392</td>
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<td>245 - 251</td>
<td>373 - 409</td>
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Table 2.9: Seam Welding Details

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<td><strong>Weld Process</strong></td>
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<td><strong>Weld Consumable</strong></td>
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<tr>
<td><strong>Pre / Postheat</strong></td>
</tr>
<tr>
<td><strong>Heat Input</strong></td>
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<tr>
<td><strong>PWHT</strong></td>
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Table 2.10: Intersection Weld Details

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<tr>
<td><strong>Weld Consumable</strong></td>
</tr>
<tr>
<td><strong>Pre / Postheat</strong></td>
</tr>
<tr>
<td><strong>Heat Input</strong></td>
</tr>
<tr>
<td><strong>PWHT</strong></td>
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Table 2.11: Load Capacity Required to Plastically Collapse Uncracked Tubular T and Y-Joints using Various Methods

<table>
<thead>
<tr>
<th>Method</th>
<th>T-Joint Axial Capacity (MN)</th>
<th>Y-Joint Moment Capacity (kN)</th>
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<tr>
<td>HSE</td>
<td>3.64</td>
<td>426</td>
</tr>
<tr>
<td>API LRFD</td>
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<td>413</td>
</tr>
<tr>
<td>API WSD</td>
<td>1.48</td>
<td>243</td>
</tr>
<tr>
<td>Pan, Plummer and Kuang</td>
<td>7.70</td>
<td>-</td>
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Table 2.12: Strain Gauge Specification

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<th>Type</th>
<th>Gauge Length</th>
<th>Resistance</th>
<th>Gauge Factor</th>
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<td>Rosette, KFG-2-120-017-11</td>
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<td>120 Ω</td>
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Table 2.13: LVDT Specifications

<table>
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<tr>
<th>Type</th>
<th>Stroke</th>
<th>Excitation Voltage</th>
<th>Sensitivity</th>
<th>Non-Linearity</th>
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<tr>
<td>Solartron DC15</td>
<td>30 mm</td>
<td>9 to 24 V d.c.</td>
<td>280 mV/mm at 10 V</td>
<td>0.5%</td>
</tr>
<tr>
<td>Solartron DC25</td>
<td>50 mm</td>
<td>9 to 24 V d.c.</td>
<td>165 mV/mm at 10 V</td>
<td>0.5%</td>
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Table 2.14: Pre-Cracking History

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<th>Specimen</th>
<th>Original Cracked Area</th>
<th>Loading Mode</th>
<th>Loading Range (kN)</th>
<th>Frequency (Hz)</th>
<th>Final Cracked Area</th>
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<tr>
<td>Y1</td>
<td>45%</td>
<td>Cyclic, OPB</td>
<td>75</td>
<td>0.8</td>
<td>70.5%</td>
</tr>
<tr>
<td>T6</td>
<td>Top Crack - 18%</td>
<td>Cyclic, Axial</td>
<td>260</td>
<td>0.8</td>
<td>25.8%</td>
</tr>
<tr>
<td></td>
<td>Bottom Crack - 0%</td>
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<td></td>
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Table 2.15(i): Digital Photogrammetry Measurements of the Change in Crack Length with Applied Loading for Test T6

<table>
<thead>
<tr>
<th>Epoch</th>
<th>Time (sec)</th>
<th>Load (kN)</th>
<th>Max. COD (mm)</th>
<th>Crack Length (mm)</th>
<th>Alignment (mm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>0</td>
<td>0</td>
<td>0</td>
<td>0.00</td>
<td>350</td>
<td>-190, 160</td>
</tr>
<tr>
<td>4</td>
<td>8</td>
<td>0</td>
<td>0.00</td>
<td>350</td>
<td>-190, 160</td>
</tr>
<tr>
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<td>0.06</td>
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<td>-190, 160</td>
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<td>56</td>
<td>111</td>
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<td>-190, 160</td>
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<td>1.45</td>
<td>350</td>
<td>-190, 160</td>
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<td>112</td>
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<td>224</td>
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<td>232</td>
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<td>252</td>
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<td>49.33</td>
<td>584</td>
<td>Crown, Crown</td>
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Table 2.15(ii): Digital Photogrammetry Measurements of the Change in Crack Length with Applied Loading for Test T1

<table>
<thead>
<tr>
<th>Epoch</th>
<th>Time (sec)</th>
<th>Load (kN)</th>
<th>Max. COD (mm)</th>
<th>Crack Length (mm)</th>
<th>Alignment (mm)</th>
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</thead>
<tbody>
<tr>
<td>0</td>
<td>0</td>
<td>0</td>
<td>0.00</td>
<td>240</td>
<td>-120, 120</td>
</tr>
<tr>
<td>1</td>
<td>9</td>
<td>50</td>
<td>0.00</td>
<td>240</td>
<td>-120, 120</td>
</tr>
<tr>
<td>10</td>
<td>86</td>
<td>718</td>
<td>0.45</td>
<td>240</td>
<td>-120, 120</td>
</tr>
<tr>
<td>19</td>
<td>163</td>
<td>1441</td>
<td>1.66</td>
<td>240</td>
<td>-120, 120</td>
</tr>
<tr>
<td>28</td>
<td>241</td>
<td>1930</td>
<td>2.46</td>
<td>240</td>
<td>-120, 120</td>
</tr>
<tr>
<td>37</td>
<td>318</td>
<td>1978</td>
<td>3.11</td>
<td>240</td>
<td>-120, 120</td>
</tr>
<tr>
<td>46</td>
<td>397</td>
<td>2100</td>
<td>3.78</td>
<td>250</td>
<td>-130, 120</td>
</tr>
<tr>
<td>55</td>
<td>473</td>
<td>2553</td>
<td>5.12</td>
<td>290</td>
<td>-155, 135</td>
</tr>
<tr>
<td>64</td>
<td>551</td>
<td>2601</td>
<td>5.73</td>
<td>320</td>
<td>-170, 150</td>
</tr>
<tr>
<td>75</td>
<td>645</td>
<td>2703</td>
<td>6.35</td>
<td>365</td>
<td>-190, 175</td>
</tr>
<tr>
<td>84</td>
<td>723</td>
<td>2947</td>
<td>8.33</td>
<td>455</td>
<td>-230, 225</td>
</tr>
<tr>
<td>93</td>
<td>800</td>
<td>1841</td>
<td>31.21</td>
<td>584</td>
<td>Crown, Crown</td>
</tr>
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</table>
Table 2.15(iii): Digital Photogrammetry Measurements of the Change in Crack Length with Applied Loading for Test T4

<table>
<thead>
<tr>
<th>Epoch</th>
<th>Time (Last 120 secs.)</th>
<th>Load (kN)</th>
<th>Crack Length (mm)</th>
<th>Alignment (mm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>894</td>
<td>0 (start of test)</td>
<td>0</td>
<td>2762</td>
<td>355</td>
</tr>
<tr>
<td>900</td>
<td>12</td>
<td>2800</td>
<td>355</td>
<td>-200 , 155</td>
</tr>
<tr>
<td>906</td>
<td>24</td>
<td>2840</td>
<td>355</td>
<td>-200 , 155</td>
</tr>
<tr>
<td>912</td>
<td>36</td>
<td>2856</td>
<td>355</td>
<td>-200 , 155</td>
</tr>
<tr>
<td>918</td>
<td>48</td>
<td>2906</td>
<td>355</td>
<td>-200 , 155</td>
</tr>
<tr>
<td>924</td>
<td>60</td>
<td>2950</td>
<td>355</td>
<td>-200 , 155</td>
</tr>
<tr>
<td>930</td>
<td>72</td>
<td>2990</td>
<td>375</td>
<td>-220 , 155</td>
</tr>
<tr>
<td>936</td>
<td>84</td>
<td>3026</td>
<td>395</td>
<td>-220 , 175</td>
</tr>
<tr>
<td>942</td>
<td>96</td>
<td>3056</td>
<td>400</td>
<td>-220 , 180</td>
</tr>
<tr>
<td>948</td>
<td>108</td>
<td>2980</td>
<td>445</td>
<td>-260 , 185</td>
</tr>
<tr>
<td>954</td>
<td>120</td>
<td>2812</td>
<td>450</td>
<td>-260 , 190</td>
</tr>
</tbody>
</table>

Table 2.15(iv): Digital Photogrammetry Measurements of the Change in Crack Length with Applied Loading for Test T2

<table>
<thead>
<tr>
<th>Epoch</th>
<th>Time (sec)</th>
<th>Load (kN)</th>
<th>Max. COD (mm)</th>
<th>Crack Length (mm)</th>
<th>Alignment (mm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>0</td>
<td>0</td>
<td>0</td>
<td>0.00</td>
<td>240</td>
<td>-110 , 130</td>
</tr>
<tr>
<td>16</td>
<td>82</td>
<td>76</td>
<td>0.10</td>
<td>240</td>
<td>-110 , 130</td>
</tr>
<tr>
<td>32</td>
<td>165</td>
<td>250</td>
<td>0.20</td>
<td>240</td>
<td>-110 , 130</td>
</tr>
<tr>
<td>48</td>
<td>247</td>
<td>1254</td>
<td>0.77</td>
<td>240</td>
<td>-110 , 130</td>
</tr>
<tr>
<td>64</td>
<td>329</td>
<td>1790</td>
<td>1.77</td>
<td>240</td>
<td>-110 , 130</td>
</tr>
<tr>
<td>80</td>
<td>412</td>
<td>2296</td>
<td>3.42</td>
<td>250</td>
<td>-115 , 135</td>
</tr>
<tr>
<td>96</td>
<td>494</td>
<td>2520</td>
<td>4.54</td>
<td>265</td>
<td>-125 , 140</td>
</tr>
<tr>
<td>112</td>
<td>576</td>
<td>2790</td>
<td>6.54</td>
<td>285</td>
<td>-140 , 145</td>
</tr>
<tr>
<td>124</td>
<td>638</td>
<td>2618</td>
<td>11.61</td>
<td>400</td>
<td>-215 , 185</td>
</tr>
</tbody>
</table>

Table 2.15(v): Digital Photogrammetry Measurements of the Change in Crack Length with Applied Loading for Test T5

<table>
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<th>Epoch</th>
<th>Time (sec)</th>
<th>Load (kN)</th>
<th>Max. COD (mm)</th>
<th>Crack Length (mm)</th>
<th>Alignment (mm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>0</td>
<td>0</td>
<td>0</td>
<td>0.00</td>
<td>260</td>
<td>-130 , 130</td>
</tr>
<tr>
<td>20</td>
<td>102</td>
<td>0</td>
<td>0.33</td>
<td>260</td>
<td>-130 , 130</td>
</tr>
<tr>
<td>40</td>
<td>204</td>
<td>400</td>
<td>0.78</td>
<td>260</td>
<td>-130 , 130</td>
</tr>
<tr>
<td>60</td>
<td>306</td>
<td>1180</td>
<td>1.95</td>
<td>260</td>
<td>-130 , 130</td>
</tr>
<tr>
<td>80</td>
<td>408</td>
<td>1820</td>
<td>3.71</td>
<td>260</td>
<td>-130 , 130</td>
</tr>
<tr>
<td>100</td>
<td>510</td>
<td>2150</td>
<td>5.55</td>
<td>280</td>
<td>-140 , 140</td>
</tr>
<tr>
<td>120</td>
<td>612</td>
<td>2550</td>
<td>8.79</td>
<td>345</td>
<td>-165 , 180</td>
</tr>
<tr>
<td>140</td>
<td>714</td>
<td>2730</td>
<td>10.62</td>
<td>375</td>
<td>-185 , 190</td>
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<tr>
<td>160</td>
<td>816</td>
<td>2840</td>
<td>16.85</td>
<td>465</td>
<td>-245 , 220</td>
</tr>
<tr>
<td>180</td>
<td>918</td>
<td>2650</td>
<td>28.55</td>
<td>584</td>
<td>Crown , Crown</td>
</tr>
<tr>
<td>196</td>
<td>1000</td>
<td>2680</td>
<td>36.61</td>
<td>584</td>
<td>Crown , Crown</td>
</tr>
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</table>
Table 2.15(vi): Digital Photogrammetry Measurements of the Change in Crack Length with Applied Loading for Test T3

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<th>Epoch</th>
<th>Time (sec)</th>
<th>Load (kN)</th>
<th>Max. COD (mm)</th>
<th>Crack Length (mm)</th>
<th>Alignment (mm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>0</td>
<td>0</td>
<td>0</td>
<td>0.00</td>
<td>260</td>
<td>-140, 120</td>
</tr>
<tr>
<td>20</td>
<td>100.6</td>
<td>366</td>
<td>0.19</td>
<td>260</td>
<td>-140, 120</td>
</tr>
<tr>
<td>40</td>
<td>201.2</td>
<td>870</td>
<td>0.60</td>
<td>260</td>
<td>-140, 120</td>
</tr>
<tr>
<td>60</td>
<td>301.8</td>
<td>1276</td>
<td>1.31</td>
<td>260</td>
<td>-140, 120</td>
</tr>
<tr>
<td>80</td>
<td>402.4</td>
<td>1900</td>
<td>1.71</td>
<td>260</td>
<td>-140, 120</td>
</tr>
<tr>
<td>100</td>
<td>503.1</td>
<td>2350</td>
<td>3.28</td>
<td>260</td>
<td>-140, 120</td>
</tr>
<tr>
<td>120</td>
<td>603.7</td>
<td>1900</td>
<td>5.22</td>
<td>290</td>
<td>-140, 150</td>
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<tr>
<td>140</td>
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<td>2790</td>
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<td>-180, 200</td>
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<td>2860</td>
<td>8.61</td>
<td>415</td>
<td>-200, 215</td>
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<td>180</td>
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<td>196</td>
<td>986</td>
<td>2894</td>
<td>28.16</td>
<td>584</td>
<td>Crown, Crown</td>
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Table 2.16: Final Results

<table>
<thead>
<tr>
<th>Specimen</th>
<th>Initial Tearing Load (kN)</th>
<th>Maximum Load (kN)</th>
<th>%Cracked Area</th>
<th>Description</th>
</tr>
</thead>
<tbody>
<tr>
<td>Y1</td>
<td>110</td>
<td>185</td>
<td>70.5%</td>
<td>510 mm long / 320 mm through</td>
</tr>
<tr>
<td>Y4</td>
<td>250</td>
<td>270</td>
<td>43.3%</td>
<td>400 mm long / 230 mm through</td>
</tr>
<tr>
<td>Y3</td>
<td>225</td>
<td>264</td>
<td>45.6%</td>
<td>400 mm long / 140 mm through</td>
</tr>
<tr>
<td>T6</td>
<td>615</td>
<td>961</td>
<td>25.8%</td>
<td>Side A 350 mm long / 150 mm through</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td>46.3%</td>
<td>Side B 520 mm long / 505 mm through</td>
</tr>
<tr>
<td>T1</td>
<td>2100</td>
<td>2977</td>
<td>16.3%</td>
<td>240 mm long / 100 mm through</td>
</tr>
<tr>
<td>T4</td>
<td>-</td>
<td>3056</td>
<td>7.8%</td>
<td>Side A 130 mm long / 14 mm deep</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td>18.0%</td>
<td>Side B 275 mm long / 100 mm through</td>
</tr>
<tr>
<td>T2</td>
<td>2296</td>
<td>2869</td>
<td>18.2%</td>
<td>Side A 240 mm long / 120 mm through</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td>17.2%</td>
<td>Side B 210 mm long / 100 mm through</td>
</tr>
<tr>
<td>T5</td>
<td>2150</td>
<td>2881</td>
<td>20.0%</td>
<td>260 mm long / 105 mm through</td>
</tr>
<tr>
<td>T3</td>
<td>2350</td>
<td>3131</td>
<td>16.3%</td>
<td>260 mm long / 65 mm through</td>
</tr>
</tbody>
</table>
Figure 2.1: Nominal Specimen Dimensions for Tubular Welded Joints
Figure 2.2: Percentage Cracked Area and Position of Each Crack for the Y-Joint Specimens
Figure 2.3: Percentage Cracked Area and Position of Each Crack for the T-Joint Specimens
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Figure 2.5: Testing Set-Up for the Y-Joint Specimens
Figure 2.6: Schematic Illustration of the T-Joint Testing Rig used for Pre-Cracking [2.9]

Figure 2.7: Static Strength Testing Set-Up for the T-Joint Specimens
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Figure 2.9: Illustrating the SCXI System Set-Up Prior to a Test
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Figure 2.11: Chord Ovalisation Measurement Assembly
Figure 2.12: The Wedge Gauge

Figure 2.13: The Cartesian Axis Selected and the Arrangement of the Retro-Reflective Targets on a T-Joint
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Figure 2.15: The Difference Between Two Images from the Initial and the Latter Stages of a Test
Camera a

Camera b

Figures 2.16a and 2.16b: Photogrammetry Images Captured Instantaneously by Cameras a and b
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Figure 2.18: Separated Brace from the Chord Following the Completion of the Test
Figure 2.19: Load-Displacement Curves for Y1

Figure 2.20: Load-Displacement Curve for Y4
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Figure 2.22: Load-Displacement Curves for T6 using Various Displacement Methods
Figure 2.23: Mode I Crack Opening Displacement at Various Loads for T6

Figure 2.24: Load-Displacement Curves for T1 using Various Displacements
Figure 2.25: Fracture Surface for Test T1

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Figure 2.29: Mode I Crack Opening Displacement at Various Loads for T2

Figure 2.30: Load-Displacement Curves for T5 using Various Displacements
Figure 2.31: Mode I Crack Opening Displacement at Various Loads for T5

Figure 2.32: Load-Displacement Curves for T3 using Various Displacements
Figure 2.33: Mode I Crack Opening Displacement at Various Loads for T3

Figure 2.34: Relationship of Normalised Load Ratio with the Percentage Cracked Area for the Tubular Joint Tests along with Mean Data from HSE
Figure 2.35: Relationship of Normalised Load Ratio with the Percentage Cracked Area [2.23]
CHAPTER 3

3.0 EXPERIMENTAL VALIDATION OF THE ULTIMATE STRENGTH OF BRACE MEMBERS WITH CIRCUMFERENTIAL CRACKS

3.1 INTRODUCTION

Welded tubular joints are considered to be the regions having the lowest ultimate strength in offshore frame structures. The joints are susceptible to high stress concentrations and fatigue cracking. These cracks will reduce the ultimate capacity of the members. The increased utilisation of flooded member detection (FMD) as a means of detecting through-thickness cracking has resulted in several investigations concerned with the residual strength of cracked tubular members [3.1-3.4].

The impetus for this study is to investigate the ultimate residual strength of full-scale circumferentially welded tubes containing cracks at the intersection. Test results show the effect of circumferential through and partial-thickness cracks on the ultimate residual strength of brace members. The data obtained can be used to examine the conservatism or otherwise of relying on FMD for crack inspection of offshore tubular members.

The results from these tests also complement the previous sets of tests completed in Chapter 2 to help better understand the consequences of large defects on the ultimate strength of tubular components.
3.2 EXPERIMENTAL DETAILS

3.2.1 Specimens
As illustrated in Figure 3.1, the test specimens consisted of two 1000 mm long, 20 mm thick tubes manufactured from BS 7191 355D steel, which were welded circumferentially according to typical North Sea procedures. Further dimensions are provided in Figure 3.2. Also shown in this figure is the exterior flange which was welded to both ends of the specimens with the purpose of load application. Table 3.1 lists the mechanical properties of BS 7191 355D [3.5].

Each of the four tubes tested contained a manufactured defect. Through-thickness flaws of different sizes were machined at the circumferential weld toe for two of the specimens. Two 14 mm deep flaws were machined into the other two specimens. All flaws were initially saw cut and were 3 mm in width. Figures 3.3a and 3.3b show the position of the saw cuts at the weld toe for the partial and through-thickness specimens. The flaws were ‘sharpened’, with the aid of finer saw cuts. The dimensions of the manufactured defects are listed in Table 3.2.

3.2.2 Fatigue Pre-Cracking
The tests were performed using a 2500 kN MTS vertical cyclic testing machine. The test rig is shown in Figure 3.4.

The purpose of the application of fatigue cycles was to sharpen the notch, taking away any effective SCF from the cut, and thus obtaining a more accurate representation of an in-service crack. The specimens were pre-cracked until a clear indication of crack growth had been detected. The initiation and propagation of the cracks was measured at regular intervals during cyclic loading. The Alternating Current Potential Difference (ACPD) technique and Magnetic Particle Inspection were employed to monitor crack growth.
Figures 3.5 and 3.6 show the fatigue crack extension of the saw cuts for a specimen containing a through-thickness crack and a partial-thickness crack, respectively. The new crack dimensions are listed in Table 3.3. Figures 3.7a, b, c, and d illustrate the cross-sectional view of the four specimens.

3.2.3 Failure Load Prediction

The R6-CODE software, which is a supplement to the R6 method established by the UK Nuclear Power Industry, was utilised to approximate the failure loads for each specimen based on its flaw size [3.6]. Failure load is defined as the load required for the initial tearing of the crack. The yield strength of the tube material quoted by the manufacturer was used. The software does not allow for the circumferential weld, and thus each specimen was treated as a tube with the appropriate circumferential flaw. Table 3.4 shows the predicted failure load for the specimens.

3.2.4 Instrumentation

An SCXI (Signal Conditioning Extensions for Installations) data acquisition system made by National Instruments was utilised. Nine out of the possible 32 channels were used to record strain values. A sampling rate of 20 points per second was chosen. The system is described in greater detail in Chapter 2, Section 2.2.5.1.

3.2.4.1 Strain Gauges

Rosette strain gauges were bonded to the surface of the tubes 500 mm from the circumferential weld to record the nominal stresses. Three gauges were equally spaced around the circumference of each specimen. The positions of the gauges are shown in Figure 3.8.

3.2.4.2 Digital Photogrammetry

Distribution of the crack opening along a section of the circumference was obtained using Vision Metrology, which is a photogrammetry based method, originally employed for the full-scale tests described in Chapter 2. The technique is used to
complement conventional instrumentation in structural testing and monitoring. In its most basic form, the method is based on following the movements of points on a specimen using a number of cameras and a computer. Its main advantages include the fact that it is a non-contacting measuring system and it can record measurements with sub-millimetre accuracy.

The technique proved the most effective and accurate available means for measuring crack opening displacement (COD) of the magnitude and range required for this project. Conventional methods, such as clip gauges, can only provide COD measurements at a single point. Furthermore, care must be taken to ensure this single point is the location for the maximum COD. If there is a greater amount of tearing from one tip of the crack, the location of the maximum COD will move from its original position. Photogrammetry can compensate for this by monitoring the entire crack.

Retro-reflective targets with an adhesive side were placed on the specimens forming a grid. A denser cluster of targets was placed on the area around the weld toe. As a reference, larger targets were placed in the camera shots, where they were deemed to be still and not affected by the movements during the test. The distance between a few of the targets was measured manually for scaling purposes and to confirm the results from the images taken.

In order to maximise the relevant data captured, each specimen was placed in the rig so that both, the centre of the defect and one of the tips could be monitored. The accuracy of the measurements depended on several factors such as the amount of lighting in the testing area, distance of the camera from the specimen and the number of cameras used. For this set of tests, two pairs of high resolution Kodak digital cameras were utilised to compensate for the relatively long distance from each camera to the specimens due to rig restrictions. The predicted accuracy using the four cameras was considered to be about ±0.5 mm.
One set of cameras, the ES1 pair, was aimed at the lower part of the crack, concerned with capturing the movements around the crack tip. The more accurate pair of 16i cameras were used to capture the centre of each notch, as well as the 'upper tip' for the shorter cracks. Care was taken as to include a set of stable targets common to all four images. This allowed the four images to be combined and therefore, provided a more accurate deformation analysis. Figure 3.9 shows a specimen prior to testing. The targets on the specimen, the connecting ends, the test machine and the back wall as well as the two pairs of cameras can be seen.

Each pair of cameras was linked to a computer for storage of the images. The two PCs were connected via a network box in order to ensure simultaneous data capture. This was crucial, as in order to correlate images from different sets of cameras, the time of capture needs to be identical.

The crack opening displacement along the defect is defined as the increase in the three-dimensional distance between a target on one side of the crack and its corresponding target on the other side. Figure 3.10 illustrates a 'before and after' case for measuring COD.

**Defining the Axes**
For all four tests, the x-axis was positioned along the length of the tube. Any movement of the unrestrained section was in the positive x-direction. The y-axis was taken as perpendicular to the tube, i.e. vertically upward. This meant that the z-axis was horizontally outwards. The Cartesian axes are shown in Figure 3.11.

Photogrammetry also provided a confirmation of the displacement measured via the LVDT of the testing actuator. The movements along the x-axis of targets placed on the connecting ends and on the stroke of the actuator were compared with the measured global displacement.
3.2.5 Static Strength Testing

The tests were performed using an Avery 12.5 MN horizontal hydraulic testing machine based at the National Engineering Laboratory in Glasgow. The specimens were clamped to the rig by means of two bell-housings which encompassed the flanges. Figure 3.9 shows a specimen in the tensile testing set-up.

The various instruments were run simultaneously to obtain datum points. The test was performed in load control mode. A suitable loading rate was selected for each test. The load was increased until the complete separation of the two parts.

A video account of the tests was recorded. The tubes were positioned so that the crack was facing towards the camera. Crack propagation could be observed, particularly during the latter stages of each test. The tests were recorded on VHS video-camera tapes and were later digitised and recorded on compact disc. These are included in Appendix B.

3.3 RESULTS AND DISCUSSION

Figure 3.12 is a photograph of two separated pieces of a specimen. It can clearly be seen that most of the tearing occurred through the parent plate, away from the circumferential weld. This was the case for all tests.

3.3.1 Test 1 (720 mm – Through-Thickness)

Test 1 was conducted at a loading rate of 200 kN per minute. The load against global displacement and maximum COD curves are shown in Figure 3.13. It can be seen that the specimen showed an almost linear elastic behaviour until a load of 2000 kN was reached. This is confirmed by the load-maximum COD curve, where a large amount of unstable tearing is indicated by the decrease in the gradient. The peak load achieved by the specimen was about 3600 kN. The tube underwent a certain amount of unstable tearing, accompanied by a drop in load of about 1000 kN.
The load bearing capacity was regained to some extent as shown by the second peak of the load-displacement curve at 3400 kN. This is significant, as it indicates the resistance of the tube to failure after extensive tearing. The specimen ultimately failed after a global displacement of 67 mm.

Figure 3.14 shows the shape of the crack during the test until the maximum load was reached. The extension of the crack tips can be observed at the higher loads.

Photogrammetry can provide displacements along any of the Cartesian axes. Figure 3.15 shows the displacements along the x-axis. The main reason for observing the x-axis movements was to have a set of data that could be compared with currently practiced techniques for measuring COD. From Figure 3.15, it is quite clear that the COD along the x-axis is almost identical to the three-dimensional values of displacement shown in Figure 3.14. This can be explained by the fact that the tubes in the testing rig are not restrained in either the ‘z’ (outward) or the ‘y’ (upward) directions. As the crack continued to tear around the weld toe and eventually into the parent metal, large movements in the z-direction as well as smaller movements in the y-direction are recorded for the targets on the tube. These displacements were, however, identical for both parts of the tube. The only difference in movement between the two parts was in the x-direction, as the x-displacement of the ‘static’ part is much smaller than the ‘dynamic’ part. Therefore, the overall displacement in the x-direction is analogous to the three-dimensional displacement. Further tests showed a similar trend and as such the three-dimensional movement was used throughout.

Figure 3.16 is similar to Figure 3.14 in that it shows the three-dimensional COD around the circumference for the first test. The distributions are plotted for load increments leading to failure. There are indications that more tearing took place from one side of the crack, as the point of maximum COD shifted along the circumference as the load was increased.
3.3.2 Test 2 (720 mm – 16 mm Deep)

A ligament thickness of about 3 mm remained after pre-cracking the second specimen. The test was again conducted at a loading rate of 200 kN per minute. From the load-displacement curves shown in Figure 3.17, it can be seen that the specimen behaved elastically until a load of approximately 3100 kN. At this point, a sudden drop in load with negligible change in displacement took place. This is thought to be the point at which the remaining section of the partial-thickness crack had fractured to become a through-thickness crack. From this point on, the load-displacement behaviour was non-linear. The load peaked at 3900 kN. This was followed by a drop in load associated with crack extension, as previously seen in Test 1. A second peak at 3100 kN can be observed on the load-global displacement curve, indicating the high load bearing capacity even after extensive tearing. The specimen catastrophically failed at a global displacement of 65 mm.

Figure 3.18 illustrates a comparison of the load-displacement curves for the tubes containing large cracks (Tests 1 and 2). It can be observed that the specimen containing the partial-thickness flaw has a lower compliance and subsequently reaches a greater maximum load.

Figure 3.19 shows the photogrammetry data collected for the second test. The crack tip is clearly identified as well as the crack centre. From Figure 3.20 it can be seen that due to tearing, the centre of the crack moved to a new position, approximately 200 mm from the original centre-line, towards the end of the test. It is also of interest to note that the set of data at a load of 3042 kN corresponds to the second peak of Test 2. This indicates that at a crack opening displacement of approximately 120 mm the specimen withstood 80% of its peak load.

The crack opening displacement data for Tests 1 and 2 are compared in Figure 3.21. This figure does not include any data for Test 2 preceding the pop-in.
3.3.3 Test 3 (180 mm – Through-Thickness)

The third specimen tested, contained a 180 mm, through-thickness crack. The loading rate selected for this test was of the order of 1000 kN per minute. Figure 3.22 shows the load versus both global displacement and the maximum COD curves for Test 3. Linear elastic behaviour is indicated by both curves until a load of about 8000 kN. At a higher value of load, the decrease in the gradient of the curves can be attributed to extensive tearing. The peak load was approximately 10500 kN. From then on a sudden increase in displacement was accompanied by a 50% drop in load. The small plateau shows that the tube which was constantly pulled and contained a very large crack could still support 40% of its maximum capacity.

Figure 3.23 shows the crack opening displacement results for Test 3. At lower loads the crack tips are symmetrical about the centre-line. At higher loads, the +120 mm COD values are larger than the −120 mm COD values, indicating more tearing from one side.

Figure 3.24 illustrates the movement of the targets on the third specimen for the duration of the test. The cracked region can be clearly identified. The coloured points on the lines tracing the movement of the retro-reflective targets represent the various stages of the test. The targets placed on the lower actuator strokes travelled in a horizontal direction and are labelled on the figure. Also shown are the stable reference targets on the back wall.

3.3.4 Test 4 (180 mm – 16 mm Deep)

Test 4 was also conducted at a loading rate of 1000 kN per minute. This specimen contained the smallest flaw of all tubes (180 mm, partial-thickness). From Figure 3.25, the load-displacement relationships are linear until a load of approximately 8000 kN is reached. There is a peak at 10500 kN followed by a sudden drop of load. A second plateau is observed at about 4000 kN leading to catastrophic failure at approximately 2000 kN. This test behaved almost identically to Test 3. This is
illustrated by Figure 3.26. The specimens show similar compliance, peak and final plateau.

Figure 3.27 shows the four load-displacement curves on one plot. Excluding Test 1, all tests show similar compliance. Although the final plateau for the two specimens with the smaller defects cannot be considered a second peak, the fact that the load drops and stabilises for a small period can be considered similar to the first two tests. It can be seen that the final sections of Tests 3 and 4 are similar to the final sections (post second peak) of Tests 1 and 2. This reiterates the idea that a tube containing a large defect can still bear a large amount of load.

The three-dimensional crack opening displacement is plotted along part of the circumference of the fourth specimen in Figure 3.28. As the defect is not through-thickness, it is reasonable to assume that part of the crack is opening more than an adjacent point. For example, the curve plotted at a load of 10484 kN is not as uniform as the curve corresponding to the maximum load of 10574 kN. The very small 'jump' in load detected at around maximum load (Figure 3.25) could be another section of the crack breaking through the metal. The non-uniformity of the crack opening for a specimen containing a partial-thickness defect can perhaps be best illustrated by Figure 3.29, which compares the two sets of crack opening displacement readings for Tests 3 and 4.

### 3.3.5 Accuracy of Load Prediction

Table 3.5 shows the initial unstable tearing and the maximum loads for the four tests. A general trend can be observed by comparing the estimated failure loads from Table 3.4 with the initial unstable tearing loads from Table 3.5. The calculated values for the partial-thickness specimens T2 and T4 are approximately 11% and 16% less conservative than the experimental values. This suggests that it is unsafe to rely on the R6-CODE for estimating the failure load of components containing circumferential partial-thickness defects. This is not the case when considering the specimens with a through-thickness flaw. The calculated values are about 50%
smaller than the experimental values. This indicates that the R6-CODE software is very conservative when analysing components with circumferential through-thickness defects. This inaccuracy in the prediction may be explained by the fact that most theoretical solutions available are concerned with partial-thickness cracks, hence the better estimates of SIF, although non-conservative.

3.3.6 Analysis of Data using the Area Reduction Procedure

The area reduction procedure was successfully implemented in Chapter 2 to provide a conservative estimate for the static strength of the cracked tubular joints. The results of the ultimate strength tests for the tubes are plotted in Figure 3.30 as the ratio of the applied net stress to the yield strength, against the percentage cracked area. The initial unstable tearing load was used in preference to the maximum load, to calculate the stress ratio on the ordinate. There were two reasons for using the initial tearing load. Firstly, it is more relevant to compare the yield strength of a material with the stress at which the component first plastically deforms. Secondly, the percentage cracked area used can be correlated to the initial tearing load and not with the maximum load, by which time the crack would have grown to a different size. However, the stress ratio was also calculated using the maximum load and the ultimate tensile strength and is provided in Figure 3.31 for purpose of completeness. The layout of the points is quite similar to Figure 3.30.

The four tests appear to follow a linear trend, parallel to the area reduction line. All tests are below the line, which renders the area reduction procedure non-conservative. However, the limited number of tests does not allow for a clear trend to be established.

The results from this study have been included in a parallel study performed by University of Manchester Institute of Science and Technology for the HSE involving similar tests on small-scale tubes containing circumferential defects [3.4]. The main, relevant conclusion of this document was that "the reduction in ultimate strength for circumferential cracks in plain or welded members cannot be predicted safely on the
basis of a linear reduction with percentage loss of cross-sectional area due to the crack.”

The area reduction procedure does not allow for the effects of extension of the crack by fracture. It only provides guidance on either unstable yielding or plastic collapse. Both fracture and plastic collapse are accounted for by the Failure Assessment Diagram procedure outlined in Chapter 1. A Failure Assessment Diagram analysis was performed for all tubular specimens and is reported on in Chapter 4.

3.4 CONCLUSIONS

Four static strength tests on pre-cracked tubes were successfully completed. The results showed that there was little difference between failure loads for through-thickness and partial-thickness cracks of the same length. The two specimens with the largest cracks displayed a distinctive second peak, even after extensive crack opening and tearing. Also, Specimen 2 reached its peak capacity after the partial-thickness crack had ‘popped in’. These results suggest that tubes containing large, through-thickness cracks are able to withstand a considerable percentage of their maximum capacity.

The predicted failure loads for through-thickness cracks, using R6-CODE, were considerably lower than the measured failure loads. The predicted loads for surface cracks were relatively close to, but higher than the measured failure loads. This could be due to the fact that there are a greater number of reliable SIF solutions for partial-thickness flaws in cylinders. While the inaccuracy of the specimens containing through-thickness flaws is greater, caution should be adhered when utilising the R6-CODE as a guide to assessing a component with partial-thickness defects as these estimations are non-conservative.
The stress ratio against the reduction in area procedure was non-conservative. Furthermore, this procedure only accounts for the plastic collapse of the component. There is a requirement for Failure Assessment Diagram analysis which accounts for both plastic collapse and fracture, to be performed. The data obtained during this study will contribute to calculating the FAD parameters presented in Chapter 4.
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<table>
<thead>
<tr>
<th>$\sigma_y$ (MPa)</th>
<th>UTS (MPa)</th>
<th>Elongation (%)</th>
<th>Charpy 'V' Notch at -50°C (J)</th>
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<td>375</td>
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Table 3.2: Initial Saw Cut Flaw Sizes

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<th>Specimen Number</th>
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<tr>
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<tr>
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<td>14</td>
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<tr>
<td>3</td>
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<td>Through-Thickness</td>
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Table 3.3: Flaw Sizes Following Pre-Cracking

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</tr>
<tr>
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</tr>
<tr>
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<td>180</td>
<td>Through-Thickness</td>
</tr>
<tr>
<td>4</td>
<td>180</td>
<td>16</td>
</tr>
</tbody>
</table>

Table 3.4: Predicted Failure Loads from R6-CODE

<table>
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<tr>
<th>Specimen Number</th>
<th>Flaw Length (mm)</th>
<th>Flaw Depth</th>
<th>R6-CODE Predicted Failure Load (MN)</th>
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<tr>
<td>3</td>
<td>180</td>
<td>Through-Thickness</td>
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<th>Initial Unstable Tearing Load (MN)</th>
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<td>Through-Thickness</td>
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<td>4</td>
<td>180</td>
<td>16</td>
<td>8.0</td>
<td>10.5</td>
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CHAPTER 4

4.0 STRUCTURAL INTEGRITY ASSESSMENT OF TUBULAR MEMBERS

4.1 INTRODUCTION

Tubular components undergo continuous loading during their service lives, resulting in the initiation and propagation of defects. The Failure Assessment Diagram (FAD) procedure described in Chapter 1 can be a useful tool for the safety evaluation of the components. Results from the tests reported in Chapter 2 and Chapter 3 are incorporated in this section to assess the T-joints and the circumferentially welded brace sections using the FAD procedure. Successful implementation of the technique requires knowledge of the stress intensity factors (SIFs).

There are only a limited number of solutions for determining the stress intensity factor at the deepest point of a crack in a tubular joints. There are no known solutions for the evaluation of SIFs for a tubular joint containing a through-thickness crack. Furthermore, solutions for pipes containing circumferential through-thickness cracks are very limited. A possible explanation for this could be that, historically, fracture mechanics assessment of a component containing a through-thickness defect has not been a priority. Such a component would most likely be either replaced or repaired using a standard technique.

A number of methods for the evaluation of the SIFs for tubular sections will be reviewed. A decision as to the most suitable technique for both, partial-thickness and through-thickness cracks will be made. The selected technique will then be validated by performing a finite element study.
A new SIF solution for tubular T-joints, containing through-thickness cracks, under axial loading will be presented. The final solution will be compared with the SIF data obtained, from a previous study on the same specimens, while the defects had not yet penetrated the chord wall.

The SIF data, the fracture toughness and mechanical properties of the materials used, and the remaining ligament of the cracked component will be integrated into producing FADs.

4.2 DETERMINATION OF Y FACTORS FOR TUBULAR SECTIONS

A number of closed form stress intensity factor solutions have been selected from the available literature to calculate Y factors for both surface and through-thickness flaws in tubular sections. A selection of solutions were considered and some were rejected as they did not extend to the very low crack aspect ratios. There is also a lack of reliable SIF solutions for through-thickness cracks under various loading conditions. Indeed, BS 7910 [4.1] contains no solutions for circumferential through-thickness flaws in cylinders under membrane loading.

The experimental results were compared with those from other studies in the formulation of empirical and semi-empirical Y factor solutions for the tubular joints.

4.2.1 Surface Flaws

Two out of the thirteen tests contained surface flaws. Figures 4.1a and 4.1b show the cross-sectional dimensions for Tube 2 and Tube 4.

BS 7910 (Section M.4.3.3.6) recommends the Newman and Raju flat plate solution [4.2] for circumferential external surface flaws in cylinders. The solution is based on an empirical equation for a surface crack in a finite plate under both bending and
tension loading, fitted to a previously modelled finite element study. In this study, the tubes were assumed as flat plates with symmetrical defects in the centre.

The more restrictive Raju and Newman solution for surface cracks in pipes [4.3] are derived from three-dimensional finite element analyses of a series of nearly semi-elliptical cracks in pipes and rods. The ensuing paper [4.3] does not provide an equation but instead gives normalised SIFs at the deepest point and at the surface point for a small number of cases. The main limiting factor in these solutions for application to this work is the minimum crack aspect ratio \((a/c)\) of 0.6. Also, the \(R_j/t\) ratio for the tubes is approximately 11.5 while the maximum \(R_j/t\) ratio quoted in the Raju and Newman paper is 10 and as such, these solutions will only be used as a form of comparison with the Newman and Raju solutions.

Figure 4.2 illustrates the variation of the stress intensity correction factor with \(a/c\), at a constant \(a/t\) of 0.8 for Tubes 2 and 4 using the Newman and Raju plate solution and the Raju and Newman pipe extrapolated solution. Also on the chart are the \(a/c\) values for both specimens. The difference between the curves in the range \(a/c\) of 1.0 to \(a/c\) of 0.6 is only a few percent. At an \(a/c\) of about 0.6, the Newman and Raju deepest point curve has a steady increase in gradient. For a smaller crack depth to length ratio, i.e. Tube 2, a very rapid rise in the Y factor values is observed. This is due to the small remaining ligament absorbing the load, and thus experiencing high stresses. The local stresses around the deepest point at high values of \(a/c\) are low. This corresponds to low normalised deepest point SIF values.

Figure 4.3 illustrates the difference between the Newman and Raju solutions at the deepest point with the surface point. The general trend of the surface point curve is an increase in the normalised SIF with \(a/c\). This increase can also be explained by considering the effects of local stresses. If \(a/c\) is close to unity, then the local stresses at the deepest point are low. However, the local stresses at the edges of the crack will be greater. As the defect length is increased the stresses around the crack 'surface points' become less intense and thus the normalised SIF decreases.
From the solutions available, the Newman and Raju flat plate solution was further considered for use with cracks in tubes by comparison with the experimental results. The main area of concern is the conservatism or otherwise of the solution bearing in mind that it was specifically designed for a plate. A method of evaluating this, is to calculate the Y factors for the through-thickness cracks in Tubes 1 and 3 (Figures 4.4a and 4.4b) and then compare the values obtained with the Newman and Raju curve. If the normalised SIF values for the through-thickness cracks are below the curve for the surface flaws, then the solution can be considered conservative. This is investigated in the following section.

4.2.2 Through-Thickness Flaws

Of the experimental tests conducted, six tubular T-joints, three tubular Y-joints and two circumferentially welded tubes contained large through-thickness cracks. Experimental crack growth data are available for the tubular T-joints until the point where the cracks became through-thickness. The data could be potentially used to correlate the following through-thickness crack solutions.

Three solutions were considered. The Zahoor equations for circumferentially cracked pipes [4.4], the Kumosa and Hull equations for circumferentially cracked cylindrical shells under uniform tensile loading [4.5] and the API RP 579 equations for cylinders with through-wall, circumferential cracks [4.6].

4.2.2.1 Zahoor Equations

The Zahoor equations are based on closed-form solutions for the energy release rate of a circumferential, through-thickness crack in a cylindrical shell under axial tension or combined bending and tension loading derived by Sanders [4.7]. From these equations, closed-form SIF solutions have been obtained. Sanders’ equations are presented in Appendix C.1.

Sanders’ closed-form solutions for the strain energy release rate of a circumferential through-thickness crack in a cylinder is calculated for “the crack extending at both
ends” [4.8, 4.7]. This suggests a centre-cracked tension (CCT) analogy, where there are two crack tips present and will make the fracture axis of the Failure Assessment Diagram approach redundant. The fracture axis is based upon the ratio of $K_f/K_{fc}$. The $K_{fc}$ term is a material property that is associated with the unstable tearing of a crack at a point and can thus not be compared with the combined $K_i$ value for both tips.

It can thus be argued that the strain energy release rate calculated is larger than the true value, which will result in a conservative SIF value. This prompted the author to conduct an in-house finite element study to evaluate SIFs for a specific pipe geometry.

**Finite Element Based Model**

The finite element package, ABAQUS [4.9], was used to establish J-integrals. These are a line or surface integral that enclose the crack front from one crack surface to the other. They are used to characterise the local stress-strain field around the crack front and can be related to stress intensity factors by the following expression:

\[
J = \frac{K^2}{E'}
\]

where:

\[
E' = \begin{cases} 
E & \text{Plane stress} \\
\frac{E}{(1 - \nu^2)} & \text{Plane strain}
\end{cases}
\]

and:

\[
E = \text{Young’s modulus} \\
\nu = \text{Poisson’s ratio}
\]

As part of the validation of the fracture mechanics finite element concept, a central crack in a rectangular steel sheet under uniform uniaxial tension was modelled. It was intended to compare the obtained SIF values with an established solution.
The finite element mesh was constructed using second order solid elements, C3D20. These elements are used when bending stress is significant, as is the case for the eventual pipe model. The density of the elements is greatest around the crack tip.

C3D20 elements were used for the entire plate, excluding the crack tip region. The elements were collapsed to 15 node triangular prisms in order to maximise the number of nodes in the vicinity of the crack tip.

The meshing was such that there were six layers of elements across the thickness of the plate to increase the confidence in the through-thickness bending stresses.

In order to make efficient use of computational time and space, only a quarter of the plate was modelled. Symmetrical boundary conditions were placed along the two lines of symmetry of the cracked plate. Each boundary condition restraint has three translational and three rotational degrees of freedom.

Uniform, uniaxial tension was applied along the x-axis, in the plane of the plate.

Figure 4.5 shows the meshed quarter-plate with the symmetrical boundary conditions and the applied loading.

As the model is six elements thick and each element has a mid-side node, there were 13 nodes and thus 13 sets of J-integral values across the crack tip. The values at the seventh node, i.e. the mid-point of the plate thickness, were taken as the most accurate J-integral values since the values at the nodes closer to the surface were deemed to have irregularities.

The J-integral values were calculated from six concentric contours, with the first contour being closest to the crack tip. In order to minimise the scatter error, the average of the two most similar values was used to obtain the stress intensity factors using Equation (4.1). Normalised SIFs were obtained from Equation (4.2).
\[ Y = \frac{K}{\sigma \sqrt{\pi a}} \]  \hspace{1cm} (4.2)

Figure 4.6 illustrates the variation of Y factor with normalised crack length, as well as depicting the notation used, for the ABAQUS model compared to a well established 2-dimensional solution concerning uniform uniaxial tension stress on a CCT geometry [4.10]. Both curves follow the same pattern. The maximum percentage difference is about 2.5%. It was decided that this result provided sufficient confidence in the method to carry out a similar model for the cylindrical pipe geometry.

A finite element model of a quarter of a steel pipe containing a through-thickness crack was created using the C3D20 elements. The use of three-dimensional elements was justified by the presence of a 3-D stress state in the tube. All modifications to the mesh as well as boundary conditions and loading were similar to the previous plate model. Figure 4.7 shows the meshed quarter-pipe with applied loading and boundary conditions. The two sets of boundary conditions parallel to the axis of the pipe are restricted from displacements in the z-direction and rotation around the x and y-axes. Figure 4.8 shows the crack tip region and the six layers of elements across the thickness.

Figure 4.9 shows the variation of the Y factors from the ABAQUS model and Zahoor solutions with the normalised crack length. The two curves resemble each other very closely. The Zahoor curve was approximately 1.5% less conservative than the ABAQUS model. The similarity of both curves in trend and range suggests that the SIF solutions originating from Sanders’ work have been validated by the FE analysis and that they can be used with a large degree of confidence.

Zahoor used the solutions from Sanders to fit a curve which enabled the calculation of SIFs based on crack angle, \( \theta \), and a pipe geometry dependent variable, A. Figure 4.10
shows the geometry associated as well as the limits to the Zahoor equations listed below.

\[ A = [0.125(R/t) - 0.25]^{0.25} \text{ for } 5 \leq R/t \leq 10 \]  
\[ A = [0.4(R/t) - 3.0]^{0.25} \text{ for } 10 \leq R/t \leq 20 \]  
\[ \text{(4.3)} \]
\[ \text{(4.4)} \]

Tension:
\[ \sigma_i = P / 2\pi Rt \]  
\[ F_i = 1 + A[5.3303(\theta/\pi)^{1.5} + 18.773(\theta/\pi)^{4.24}] \]  
\[ K_i = \sigma_i \sqrt{\pi R \theta} F_i \ (R/t, \theta /\pi) \]  
\[ \text{(4.5)} \]
\[ \text{(4.6)} \]
\[ \text{(4.7)} \]

Bending:
\[ \sigma_b = M / \pi R^2 t \]  
\[ F_b = 1 + A[4.5967(\theta/\pi)^{1.5} + 2.6422(\theta /\pi)^{4.24}] \]  
\[ K_i = \sigma_b \sqrt{\pi R \theta} F_b \ (R/t, \theta /\pi) \]  
\[ \text{(4.8)} \]
\[ \text{(4.9)} \]
\[ \text{(4.10)} \]

where:
\[ A = \text{Geometry dependent property} \]
\[ \theta = \text{Crack half-circumferential angle} \]
\[ \sigma = \text{Remote applied stress} \]
\[ F = \text{Normalised stress intensity factor} \]
\[ K_i = \text{Mode I stress intensity factor} \]

4.2.2.2 Kumosa and Hull Equations

The Kumosa and Hull equations [4.5] were produced by utilising a finite element package to model a quarter of a tube, containing a circumferential through-thickness crack of half length ‘a’. They are the recommended solutions referenced by the Stress Intensity Factors Handbook [4.11]. The one-element thick model consisted of 190
isoparametric 3-D elements and is shown in Figure 4.11. The opening stress intensity factor, $K_t$, is represented in Equation (4.11).

$$K_t/K_o = G_m + G_b (2y/t)$$

(4.11)

where $G_m$ and $G_b$ are non-dimensional membrane and bending stress components respectively, $t$ is the thickness of the tube and the co-ordinate, $y$, is equal to $+t/2$ on the outer surface and $-t/2$ on the inner surface of the tube. The stress intensity factor, $K_0$, is representative of a centre-cracked plate subjected to similar boundary conditions and loading as the circumferentially cracked tube and is given by:

$$K_o = \sigma_m \sqrt{\pi a} \left[ \frac{2b}{\pi a} \tan \left( \frac{\pi a}{2b} \right) \right]^{1/2}$$

(4.12)

where:

- $\sigma_m$ = Nominal stress
- $2b$ = Cylinder circumference

The stress intensity components $G_m$ and $G_b$ are dependent on loading condition, specimen geometry, and Poisson’s ratio, $v$, and can be evaluated by calculating a dimensionless parameter, $\lambda_2$, and reading off the set of charts provided.

$$\lambda_2 = \left[ 12(1-v^2) \right]^{1/2} \frac{a}{\sqrt{Rt}}$$

(4.13)

Figures 4.12a and 4.12b are extracts from the two charts [4.5] used to read off the membrane stress component. As it can be seen, the data provided are restricted to a ratio of mean radius to pipe thickness ($R/t$) of 25 for Figure 4.12a and a half crack angle of $20^\circ$ for Figure 4.12b.
To allow comparison of similar quantities between this solution and other established solutions, appropriate calculations were conducted for a uniaxial tension case as shown in Equations (4.14-4.17).

\[
K_1 = \sigma_m Y \sqrt{\pi a} \tag{4.14}
\]

\[
K_0 = \sigma_m Y_0 \sqrt{\pi a} \tag{4.15}
\]

\[
K_1/K_0 = Y/Y_0 = G_m \tag{4.16}
\]

\[
Y = Y_0 G_m = \left[ \left( \frac{2b}{\pi a} \right) \tan \left( \frac{\pi a}{2b} \right) \right]^{1/2} G_m \tag{4.17}
\]

Y factor values were then calculated for various crack lengths at an R/t of 25. The obtained values were then compared with the flexible Zahoor solutions for R/t of 5, 10, 12, 15 and 20 as shown in Figure 4.13. The general trend of the Kumosa and Hull curve matches the Zahoor curves. Figure 4.14 compares the change in Y factor with the increase in R/t for a half crack angle of 20° for the Kumosa and Hull and the Zahoor solutions. Once again, the general trend is the same, with the Zahoor solutions being about 10% more conservative.

Of the two methods, the Flexible Zahoor solutions are preferred to the limit restricted Kumosa and Hull solutions.

4.2.2.3 API RP 579 Equations

The equations from Appendix C of the API RP 579 document [4.6] are listed below. Figure 4.15 shows the geometry concerned for a circumferential through-wall crack in a cylinder.

\[
K_1 = (M_m \sigma_m + M_b \sigma_b) \sqrt{\pi c} \tag{4.18}
\]

\[
M_m = \max[(A_{mm} + A_{mb}), (A_{mm} - A_{mb})] \sqrt{\frac{2R}{c}} \tan \left( \frac{c}{2R} \right) \tag{4.19}
\]
\[ M_b = \max[(A_{bm} + A_{bb}), (A_{bm} - A_{bb})] \]  

(4.20)

where constants \( A_{nm}, A_{mb}, A_{bm} \) and \( A_{bb} \) are calculated from the equations provided in the reference.

Solutions are provided for \( R/t \) values of 3, 5, 10, 20, 50 and 100. The ratio of the inner radius to the thickness for Tubes 1 and 3 is 11.5. A curve was fitted to the calculated constants \( A_{nm} \) and \( A_{mb} \) for \( R/t \) values of 5, 10 and 20. The constants were then read off for an \( R/t \) value of 11.5.

Table 4.1 provides the membrane correction factors as well as the mode I stress intensity factor for Tube tests 1 and 3 using the Zahoor and the API solutions. Figure 4.16 compares the four solutions together. It can be seen that the API RP 579 normalised SIF values are consistently more conservative than the Zahoor data. These results are shown in Figure 4.17 to allow for comparison of the through-thickness data with the surface flaws. In addition, Figure 4.17 shows the Newman and Raju deepest point and surface point curves for a constant \( a/t \) of 0.8 and a varying \( a/c \), along with the four calculated points for Tubes 1 and 3. The values for \( a/c \) for the through-thickness cracks were taken as the ratio of the tube thickness (20 mm) to the crack half length. Three of the four points for the through-wall cracks fall below the Newman and Raju curve. This indicates the likelihood that the Newman and Raju curve is conservative.

The through-wall normalised SIF solutions are for the 'centre of the wall' point of the tubes. The two points for the larger, through-thickness cracks on Figure 4.17 do not match favourably to the Newman and Raju surface point solution. This is not of great concern as the study is geared towards producing two different models. The first is valid until the point where the crack becomes through-thickness and the second is a model for through-thickness cracks.
4.3 SIFs DERIVED FROM EXPERIMENTAL FATIGUE DATA FOR SURFACE CRACKS

As mentioned previously, lack of reliable SIF data for large, through-thickness cracks in tubular joints has limited the use of structural integrity assessment procedures. The aim of this part of the study is to create a fracture mechanics model for this type of component and defect geometry, and to correlate the data as an extension to the experimental SIF values obtained for the surface cracks. The new solution can then be compared to the data points obtained, for the through-thickness cracks in T-joints, from the Zahoor equations discussed in the previous section.

The general practice calls for a tubular joint failure to be defined in terms of through-thickness cracking. For this reason, design criteria such as fatigue crack growth and fatigue life are normally expressed in relation to the crack depth.

The SIF solutions for through-thickness cracks are based on crack length. Therefore, in order to compare and correlate SIFs for through-thickness cracks with surface cracks, a crack length based solution is required. Dover et al. [4.12] proposed the idea of replacing the crack depth variable with half the crack length to obtain experimental SIFs at the crack tips.

Nui et al. [4.13] followed on from this idea and conducted experiments on T-joints under axial, out-of-plane bending and in-plane bending loading in order to find experimental Y factors using the crack length. They employed the empirical approach developed by Dover et al. [4.14]. In this method, the experimentally measured crack (depth) growth rate is used to calibrate the corresponding SIF range. Equations (4.21) to (4.23) describe the basis for this approach.

Equation (4.21) is the Paris and Erdogan relationship [4.15]:

\[ \frac{d a}{d N} = C (\Delta K)^m \]
where: \( \frac{da}{dN} \) is the fatigue crack growth rate (m/cycle)

'\( C \)' and '\( m \)' are Paris constants

\( \Delta K \) is the stress intensity factor range

Also,

\[ \Delta K = \Delta \sigma Y_{\exp} \sqrt{\pi a} \]  

(4.22)

where \( \Delta \sigma \) is the hot spot stress range.

Therefore:

\[ Y_{\exp} = \left( \frac{1}{C} \frac{da}{dN} \right)^{\frac{1}{m}} \frac{1}{\Delta \sigma \sqrt{\pi a}} \]  

(4.23)

Nui et al. Replaced the crack depth term, \( a \), with half crack length, \( c \), to give the experimental \( Y \) factors at the crack tip as shown below:

\[ Y_{\exp} = \left( \frac{1}{C} \frac{dc}{dN} \right)^{\frac{1}{m}} \frac{1}{\Delta \sigma \sqrt{\pi c}} \]  

(4.24)

They argued that the use of the crack length has the advantage of being compatible with the majority of inspection recordings. Their experimental data were not compared with known theoretical models.

It was decided to use the crack growth data available from the tests performed by Myers [4.16] on the axially loaded T-joints to calculate \( Y \) factors.
From the raw ACPD data, and by assuming any depth reading over 0.5 mm as a defect, crack length against the number of cycles were plotted. Figure 4.18 is the crack length growth curve for T3. If the joint contained more than one defect, the larger of the two was considered.

The 3-point incremental polynomial method [4.17] was used to derive crack growth rates \( \frac{dc}{dN} \) from the 'crack length against number of cycles' curves. The method requires fitting a polynomial to a segment of the data points. The data segments consist of an odd number of elements, in this case, three. The growth rate equals the slope of each polynomial.

The SIFs obtained are dependent on the accuracy of the Paris Law constants, \( C \) and \( m \). These values were determined from compact tension (CT) specimens manufactured from the parent plate SE 702 material. The tests were performed in air by Creusot Loire Industries [4.18] and are represented in Equation (4.25).

\[
\frac{da}{dN} = 2.72 \times 10^{-12} (\Delta K)^{1.53} \quad (4.25)
\]

The stress range used for normalisation in Equation (4.24) is traditionally the hot spot stress range. However, for this application, the hot spot stress term is somewhat redundant, as there is a greater interest in the surface points of larger cracks. Also, the proposed through-thickness SIF model will be dependent on a nominal stress based model.

Figure 4.19 illustrates the change in experimental Y factor with \( c/b \) for T5, where 2b is half the brace/chord intersection length. The general trend is as expected, with high surface SIF values at low ratios of \( c/b \) due to fact that the crack growth rate is governed by the hot spot stress. The Y factor values tend to decrease with reduction in local stresses corresponding to an increase in crack length.
4.4 EXISTING SIF SOLUTIONS FOR THROUGH-THICKNESS CRACKS IN TUBULAR MEMBERS

The Zahoor solutions for circumferential through-thickness cracks in a cylinder under tension, which were introduced earlier in this chapter, were used to estimate the tubular joint SIFs (Equations (4.7) and (4.10)). As for all attempted SIF solutions, only the larger crack was considered. There is no requirement for a stress distribution. The stress used in Equation (4.7) was the nominal stress at the point of initial tearing of the crack. Due to a problem with T-joint test T4, the initial tearing load was not available and as such, a SIF point could not be calculated. In all cases, the half-crack angle used in the Zahoor equations was representative of the half-surface crack length indicated in Table 2.16.

Figure 4.20 shows the five SIF points obtained from the Zahoor equations along with the experimental SIF points for the surface crack for T-joint test T5, against c/b. The a/t = 1 line indicates the point at which the surface crack in T5 became a through-thickness crack.

The small number of data points, along with the lack of variation in crack length, make it difficult to establish a definite trend in the through-thickness SIF data points. In general, the four Zahoor points for the four joints with similar crack sizes (T1, T2, T5, and T3) seem to be approximately in the same region where the experimental Y factor values end. The high Y factor value for T6 can be explained by the fact that there was very little material left at a c/b of 0.95. This will result in very high local stresses and thus a high 'surface point' SIF.

In must not be forgotten that the Zahoor solutions were produced for circumferentially cracked pipes and should only be used to estimate SIFs for tubular joints if no other solutions exist. The next section describes the development of a stress intensity factor solution aimed at axially loaded tubular T-joints containing through-thickness defects.
4.5 NEW SIF MODEL FOR THROUGH-THICKNESS CRACKS IN TUBULAR T-JOINTS

The solution for a central crack in a rectangular sheet under uniform uniaxial tensile stress [4.10] was selected as a skeleton solution (Figure 4.21). It was intended that this solution be applied to a through-thickness crack in a tubular joint by incorporating Albrecht and Yamada’s non-uniform stress concentration factor, $Y_g$ [4.19].

The method was successfully employed by Myers [4.16] and Monahan [4.20] to obtain a non-uniform stress correction factor for surface cracks. Figure 4.22 shows the basis of this factor applied for a through-thickness crack in a sheet. The basic requirement is knowledge of the stress distribution ($\sigma_{xi}$) along the anticipated crack path of the uncracked body. A crack of a given length is then inserted and is divided into $n$ increments. Each increment ($x_i$) is subjected to the corresponding stress determined previously. The correction factor is then calculated from the relationship below, which is based on a solution for a central crack in an infinite plate with two equal forces, $P$, applied at a distance $x$ from the crack centre-line (Figure 4.23).

$$Y_g = \frac{2}{\pi} \sum_{i=1}^{n} \frac{\sigma_{xi}}{\sigma} \sin^{-1} \left( \frac{x_{i+1}}{a} \right) - \sin^{-1} \left( \frac{x_i}{a} \right)$$  \hspace{1cm} (4.26)

where $\sigma_{xi}/\sigma$ is the ratio of the stress at a point to the nominal stress and $a$ is half the crack length. The correction factor can then be calculated by repeating the above procedure for the range $0 \leq c/b \leq 1$.

The following section deals with the determination of the stress along the anticipated crack path.
4.5.1 Stress Analysis of a Tubular Joint

The complex geometry of a tubular joint gives rise to a complex stress field along the proposed crack path. Early studies attempted to overcome this by different forms of simplification of the stress field. One of the earliest methods employed, assumed the stress distribution around the intersection to be equal to the hot spot stress in combination with a through-thickness stress which is composed of a bending and a membrane component [4.21]. A criticism of this stress distribution is that it takes no account of the decreasing stress field along the intersection, away from the hot spot stress site. The use of this stress distribution in the formulation of the stress intensity factor will produce very conservative SIFs.

A follow on method involved averaging the stress distribution around the intersection using the average stress distribution derived by Dharamavasan [4.22, 4.21]. The stress used was an average stress around the intersection plus the through-thickness stress. The method, however, places too much emphasis on stresses away from the hot spot stress site.

The third method is an alternative stress distribution which averages the stress weighted for the distance from the hot spot stress site, to any position around the intersection. This is the weighted average stress approach [4.21].

The three simplified stress distributions are combined with established SIF solutions [4.23] to provide SIF results.

Figure 4.24 illustrates the typical stress distribution for a tubular joint and compares it with the above methods.

More recently, Chang [4.24] proposed various closed form solutions enabling the evaluation of the stress distribution along the intersection of tubular T, Y, X, and DT-joints. The study was based on thin shell finite element analysis on 330 tubular joints using the versatile ABAQUS package [4.9]. Parametric equations were derived for
the average stress concentration factors in the four types of tubular joints under various loading. Figure 4.25 shows the stress distributions obtained from Chang's model for the three modes of loading acting on a T-joint.

In order to apply Chang's distributions to the non-uniform stress concentration factor and subsequently the uniform, uniaxial tensile stress model, there is a requirement for the elimination of the bending stress component from the total stress. The two stress components may be separated by the use of the Degree of Bending (DoB) at a particular point, as shown below:

\[
\text{DoB} = \frac{\sigma_B}{\sigma_T} \tag{4.27}
\]

\[
\sigma_T = \sigma_B + \sigma_M \tag{4.28}
\]

\[
\sigma_T (1-\text{DoB}) = \sigma_T - \sigma_B = \sigma_M \tag{4.29}
\]

where:

- \(\sigma_T\) = Total stress
- \(\sigma_B\) = Bending stress
- \(\sigma_M\) = Membrane stress

Unfortunately, Chang only provides the DoB at the crown and saddle points. A search of available literature did not yield any solution for determining the DoB around the intersection \[4.25\]. There was also concern over the fact that the weld was not modelled. This meant that the effects of the notch stresses were ignored.

These factors prompted the search for alternative methods to provide membrane stresses along the intersection of a tubular joint.

A second study, recently conducted by Uddin \[4.26\], was examined for suitability in providing the required stress distributions. Uddin modelled the brace and the chord of the tubular welded sections with 20 node quadratic brick elements, while the weld comprised of extruded triangular elements.
This model, also constructed in ABAQUS, allowed the membrane stresses to be calculated from one crown point to the next, for a DT-joint with a single brace loaded axially. Figure 4.26 illustrates the resultant stresses along the intersection at the ‘top’, ‘middle’ and ‘bottom’ positions of the elements along the weld toe. The ‘top’ stress is a combination of the bending and membrane stresses, while the ‘middle’ stress is the pure membrane stress.

Table 4.2 compares the SCFs, for a tubular T-joint under axial load, by using existing methods. The average experimental SCF was found from the previous study by Myers [4.16]. It can be seen that the SCF from Chang’s model is very conservative, while the SCF from Uddin’s DT-joint model is very close to the experimental result. None of the other methods provided a stress distribution along the intersection.

The fact that the presence of the second brace on a DT-joint hinders the deformation of the chord wall and results in smaller geometric stresses when compared to a T-joint is well understood. Therefore, in order to achieve a higher degree of confidence in Uddin’s DT model, the Chang stress distribution for a T-joint was compared to the identically modelled Chang distribution for a DT-joint. The results are presented in Figure 4.27. The two curve show the same general trend. The difference of the two peak stresses at the saddle point is about 5%. It is postulated that this difference is small enough to draw a parallel comparison between Uddin’s DT model and the stress distribution that would be obtained from a T-joint modelled similar to Uddin’s model.

It was decided that Uddin’s model would be used to evaluate the stress distribution around the weld toe.

The normalised stress distribution was used to evaluate the $\sigma_n/\sigma$ term in Equation (4.26) to obtain the non-uniform stress concentration factor. This was then combined with the solution for a central crack in a rectangular sheet under uniform uniaxial tensile stress to develop a new $Y$ factor for a through-thickness crack in an axially loaded T-joint, from here on referred to as $Y_T$. A curve was fitted to the relationship.
of various values of $Y_T$ against $a/c$. The equation for this curve is provided in Appendix C.2.

Figures 4.28 to 4.33 illustrate the variation of $Y_T$, $Y_{exp}$ and the SIFs from Zahoor with $c/b$ for all T-joints. The point at which the defect becomes a through-thickness crack is marked on each chart. The analytical line representing $Y_T$ is only valid past this point. In general, the commencement of the analytical curve coincides with the end of the experimental data points. This provides a degree of confidence to the theoretical SIF solution.

The shape and range of the line is further correlated by the points obtained from the Zahoor equations. The $Y_T$ curve is consistently more conservative.

The $Y_T$ solution will be utilised to calculate $K_I$ values for all T-joints. The corresponding $K_I$ values for the Y-joints are calculated from the Zahoor solutions for a cracked cylinder under bending loading. These will then be combined with fracture toughness data and used for structural integrity assessment by the Failure Assessment Diagram method.

### 4.6 FAD ANALYSIS

The Failure Assessment Diagram method enables the significance of the defects to be evaluated by considering both fracture and plastic collapse of the component. Results of the assessment lead to precautionary actions on a fitness-for-purpose basis regarding the safety of the component containing known or postulated defects.

The calculation methods for the two parameters, $K_r$ and $L_r$, which were defined in Chapter 1, will be discussed in the following section. It should be noted that while evaluating $K_I$ (and thus $K_r$) for a specimen containing more than one crack, only the larger crack was considered. This is due to the fact that SIFs are concerned with the
stress intensity at the most highly stressed crack tip. However, all crack areas were used when calculating $L_r$ as the remaining ligament is the governing factor of the load ratio axis. Furthermore, taking only one cracked side into account would substantially under estimate the primary net section stress.

### 4.6.1 Circumferentially Welded Tubes

The fracture parameter, $K_f$, is evaluated by the ratio of the stress intensity factor to the fracture toughness of the material, i.e. Equation (1.10). The SIFs were calculated by using the Zahoor equations.

It is very challenging to experimentally measure the fracture toughness of a tough, medium strength steel, such as BS 7191 355D, at room temperature. This is mainly due to the fact that the minimum requirement for material thickness is based on the equation below, provided in BS 7448 [4.27].

$$B = 2.5 \left( \frac{K_{Ic}}{\sigma_y} \right)^2$$

where: 
- $B$ = Specimen thickness 
- $K_{Ic}$ = Fracture toughness 
- $\sigma_y$ = Yield strength

In order to calculate an estimate for the fracture toughness, the relationship between the critical CTOD ($\delta_{Ic}$) and the critical SIF ($K_{Ic}$), noted in Equation (1.12), is modified to:

$$\delta_{Ic} = \frac{K_{Ic}^2}{\sigma_y E}$$

where: 
- $\delta_{Ic}$ = 0.5 mm [4.28]
\[ E = \text{Modulus of elasticity} \]

This relationship between material properties provides a value of 193.1 MPa m\(^{1/2}\) for the estimated fracture toughness. Therefore, the minimum required specimen thickness, \(B\), is approximately 663 mm, which is not feasible to test. Furthermore, the material available from the separated tubular sections was 20 mm in thickness.

Two suggested ways of overcoming this problem are discussed below. The BS 7910 recommends the use of Charpy V-notch tests to provide fracture toughness values and BS 7448 advises the use of side-grooves. Both methods were dismissed for the reasons below:

Although proper sized Charpy V-notch specimen could be obtained from the tubular sections, the impact test is not thought to be a good provider of \(K_{lc}\) data. As the notches are generally produced by using a cutter on a milling machine, no two specimens can be considered the same, thus accurate comparisons cannot be made. In contrast, the pre-cracking stage of the \(K_{lc}\) tests, ensures that all specimen are of similar crack tip acuity. A Charpy test is also based on a stress wave travelling through the material caused by an impact. This is a totally different mechanism to that of a \(K_{lc}\) test where the stresses are quasi-static in nature. Indeed, the Charpy V-notch tests can be classified as qualitative while the fracture toughness tests may be considered quantitative.

The material near the outer surface of a relatively thin specimen is in a state of low triaxiality, which indicates a plane stress condition. Side grooves provide uniform distribution of stresses through the thickness and constrain the plane stress conditions near the free surface of the tested specimen. This method is used for quality assurance of pressure vessel materials [4.29, 4.30].

Freed [4.31] tested many single edge-notched specimens made from high strength steels and aluminium and titanium alloys. He compared the \(K_{lc}\) values obtained from
side-grooved specimens with smooth specimens and found a difference of 1% to 3% between the two types.

Work by Zhang [4.29] on a series of tensile tests performed on precracked, Charpy sized specimen showed the importance of the depth of the grooves. He suggested that for certain materials, there exists an optimal side-groove depth from which the specimen gains the maximum additional thickness. There was, however, no recommendation as to the number of tests required to obtain the optimal depth of the side-grooves. It would be interesting and useful to find the optimal depth for specimens manufactured from BS 7191 355D. This, however, is outside the scope of this study.

It was decided that the estimation from the critical CTOD conversion would be used as the final value of $K_{IC}$. The $K_r$ values are listed in Table 4.3.

The plastic collapse parameter was initially calculated by using Equation (1.19). This, however, proved problematic as the $2a/\pi r$ value for the first specimen was out of the limit range stated in Section P.4.2.2 in BS 7910. This rendered the calculation for the reference stress, $\sigma_{ref}$, invalid. Furthermore, the $\sigma_{ref}$ values were of a very high order and subsequently resulted in $L_r$ values much greater than $L_{rmax}$. These values are shown in Table 4.3. It was therefore decided that the PD 6493 [4.32] definition of $L_r$, that being the ratio of the net section stress to the yield strength, stated in Equation (1.18), be used to calculate the plastic collapse parameter. The net section stress is defined as the ratio of the initial tearing load to the remaining ligament area. These final $L_r$ values are shown in Table 4.3.

The values from Table 4.3 are plotted on a FAD displayed in Figure 4.34. It can be seen that the two specimens with the smaller defect sizes are in a similar region. The larger values of $L_r$ are indicative of the greater amount of resistance to plastic collapse during these two tests. Also, the points for the two specimens with through-thickness
cracks fall outside the envelope. The large value of $K_r$ for Tube1 may be attributed to the inaccuracy of the estimated value of the fracture toughness discussed above.

### 4.6.2 Tubular Joints

Only the initial tearing load condition was considered. The fracture parameters were evaluated by utilising the $Y_T$ solution (described in Section 4.5), the nominal brace stress corresponding to the initial tearing load, and half the initial crack length. The fracture toughness was quoted as 279 MPa.m$^{1/2}$ for the parent plate [4.33]. Using Equation (4.30), the minimum required thickness was calculated as 409 mm. Due to the uneconomical size of the specimen required, no valid fracture toughness data could be produced. As such, the $K_{ic}$ value provided by the manufacturer was utilised. The $K_r$ values for the initial tearing load condition are presented in Table 4.4.

The collapse parameter, $L_r$, was calculated using Equation (1.22). The uncracked joint capacity, $P_c$, was calculated using the API RP 2A LRFD equations [4.34] mentioned in Chapter 1. This set of equations was preferred to the HSE guidance equations due to the fact that they are based on the observance of first cracking. The area reduction factor, $F_{AR}$, is calculated from Equation (1.9). The $L_r$ values for the initial tearing load condition are presented in Table 4.4.

Figure 4.35 illustrates the FAD for the T-joints. All points fall outside the FAD envelope. Also, the five points lie between a range of $K_r$ values of 0.6 to 0.8. This indicates that the steel has a relatively good resistance to fracture in the welded form. All $L_r$ values are greater than the calculated $L_{rmax}$ value. It is recommended in Annex P of BS 7910 that in case of $L_r > L_{rmax}$, the flaw needs to be recharacterised. Unfortunately, the recommendations for flaw recharacterisation do not include through-thickness defects. The cause of the magnitude of the $L_r$ values can be attributed to the conservatism of the uncracked solutions provided by the API guidance. As for most design guidance equations, inherent safety factors would have been applied for the purpose of conservatism.
4.7 CONCLUSIONS

Methods for evaluating the stress intensity factors of tubular components have been presented.

Several existing SIF solutions for tubular sections have been reviewed. The most suitable technique for both, partial-thickness and through-thickness cracks has been chosen. The selected technique was then validated by performing a finite element based study.

A new SIF solution for tubular T-joints, containing through-thickness cracks, under axial loading has been presented. The method is based on the SIF at the crack tip and the non-uniform stress distribution present in an axially loaded tubular T-joint. The final solution was compared with the SIF data obtained, from a previous study on the same specimens, while the defects had not yet penetrated the chord wall. The two sets of data showed a good correlation.

The stress intensity factor solutions and results from the tests reported in Chapter 2 and Chapter 3 have been used to evaluate the FAD parameters. All tubular components have been analysed on the appropriate FAD. The unavailability of experimental fracture toughness values for BS 7191 355D steel may have caused inaccuracies when analysing the circumferentially welded tubes. The relatively high $K_I$ values obtained when assessing the tubular joints indicated that the steel has a good resistance to fracture in welded form. The large $L_r$ values could be due to the conservatism of the equations used to evaluate the uncracked strength. It would be of great interest to conduct full-scale static strength tests on uncracked specimens in order to evaluate this presumed conservatism and amend the FAD.
4.8 REFERENCES


4.9 TABLES AND FIGURES

Table 4.1: Membrane Correction Factor and the Mode I Stress Intensity Factor for the Circumferentially Welded Tube Tests 1 and 3

<table>
<thead>
<tr>
<th>Specimen</th>
<th>Half Crack Angle</th>
<th>Membrane Correction Factor</th>
<th>Using Initial Tearing Stress</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td>$F_t$ (Zahoor)</td>
<td>$M_m$ (API 579)</td>
</tr>
<tr>
<td>T1</td>
<td>$\pi/2$</td>
<td>4.33</td>
<td>5.85</td>
</tr>
<tr>
<td>T3</td>
<td>$\pi/8$</td>
<td>1.28</td>
<td>1.46</td>
</tr>
</tbody>
</table>

Table 4.2: Comparison of SCFs using various Methods for a Tubular Joint under Axial Loading

<table>
<thead>
<tr>
<th>SCF Method (Joint Type)</th>
<th>SCF Chord Saddle</th>
<th>SCF Chord Crown</th>
</tr>
</thead>
<tbody>
<tr>
<td>Chang (T)</td>
<td>16.38</td>
<td>4.52</td>
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<tr>
<td>HCD (T)</td>
<td>17.95</td>
<td>4.21</td>
</tr>
<tr>
<td>UEG (T)</td>
<td>14.15</td>
<td>4.86</td>
</tr>
<tr>
<td>W &amp; S (T)</td>
<td>13.92</td>
<td>4.86</td>
</tr>
<tr>
<td>E &amp; D (T) [Fixed]</td>
<td>12.39</td>
<td>3.70</td>
</tr>
<tr>
<td>E &amp; D (T) [Pinned]</td>
<td>13.72</td>
<td>4.99</td>
</tr>
<tr>
<td>Kuang (T)</td>
<td>12.40</td>
<td>-</td>
</tr>
<tr>
<td>Gibstein (T)</td>
<td>13.96</td>
<td>-</td>
</tr>
<tr>
<td>Av. Experimental (T)</td>
<td>11.50</td>
<td>-</td>
</tr>
<tr>
<td>Chang (DT)</td>
<td>15.54</td>
<td>2.29</td>
</tr>
<tr>
<td>Uddin (DT)</td>
<td>11.42</td>
<td>1.52</td>
</tr>
</tbody>
</table>

Table 4.3: FAD Parameters for the Circumferentially Welded Tubes

<table>
<thead>
<tr>
<th>Specimen</th>
<th>$K_r$</th>
<th>$L_r$ (PD 6493)</th>
<th>$L_r$ (BS 7910)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Tube 1</td>
<td>1.78</td>
<td>0.35</td>
<td>Out of Range</td>
</tr>
<tr>
<td>Tube 2</td>
<td>0.63</td>
<td>0.46</td>
<td>2.62</td>
</tr>
<tr>
<td>Tube 3</td>
<td>0.84</td>
<td>0.81</td>
<td>1.07</td>
</tr>
<tr>
<td>Tube 4</td>
<td>0.55</td>
<td>0.79</td>
<td>1.68</td>
</tr>
</tbody>
</table>
Table 4.4: FAD Parameters for the T-Joint Specimens

<table>
<thead>
<tr>
<th>Specimen</th>
<th>$K_r$</th>
<th>$L_r$</th>
</tr>
</thead>
<tbody>
<tr>
<td>T6</td>
<td>0.60</td>
<td>1.36</td>
</tr>
<tr>
<td>T1</td>
<td>0.70</td>
<td>1.20</td>
</tr>
<tr>
<td>T2</td>
<td>0.77</td>
<td>1.75</td>
</tr>
<tr>
<td>T5</td>
<td>0.73</td>
<td>1.26</td>
</tr>
<tr>
<td>T3</td>
<td>0.80</td>
<td>1.38</td>
</tr>
</tbody>
</table>
Figure 4.1a: Tube 2, 720 mm, 16 mm Deep Flaw, 
(a/t = 0.8, a/c = 0.0444, 
R_i = 0.23 m, t = 0.02 m)

Figure 4.1b: Tube 4, 180 mm, 16 mm Deep Flaw, 
(a/t = 0.8, a/c = 0.1778, 
R_i = 0.23 m, t = 0.02 m)

Figure 4.2: Comparison of the N-R Flat Plate Solutions with the R-N Pipe Solutions at the Deepest Point for Tests T2 and T4 at a/t of 0.8
Figure 4.3: Comparison of Deepest Point and Surface Point N-R Flat Plate Solutions for Tests T2 and T4 at a/t of 0.8

Figure 4.4a: Tube 1, 720 mm, Through-Thickness (a/c = 0.0556, R_i = 0.23 m, t = 0.02 m)

Figure 4.4b: Tube 3, 180 mm, Through-Thickness (a/c = 0.222, R_i = 0.23 m, t = 0.02 m)
Figure 4.5: A Meshed Quarter-Plate with Symmetrical Boundary Conditions and Applied Loading

Figure 4.6: Comparing the Y Factors from the ABAQUS Model with Rooke and Cartwright for a CCT Geometry
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Figure 4.8: A close-up of the Meshed Region Surrounding the Crack
Figure 4.9: Comparing the Y Factors from the ABAQUS Model with Zahoor Solutions for a Pipe of R/t = 12

Figure 4.10: Through-Wall Crack in a Cylinder Investigated by Zahoor under Remote Tension and Remote Bending

\[
\begin{align*}
5 \leq R/t &\leq 20 \\
0 < \theta/\pi &\leq 0.55
\end{align*}
\]
Figure 4.11: Finite Element Model of One Quarter of a Cylinder with $\alpha = 20^\circ$ and $R/t = 25$ from Kumosa and Hull [4.5]

Figure 4.12a: Membrane Stress Intensity Components, $G_m$, for Cylinders with $R/t = 25$ and $\alpha$ Ranging from $10^\circ$ to $60^\circ$
Figure 4.12b: Membrane Stress Intensity Components, $G_{m}$, for Cylinders with $\alpha = 20^\circ$ and R/t Ranging from 5 to 20

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Figure 4.14: Comparison of SIFs from Kumosa and Hull (3-D, Plane Stress) with Zahoor for a Circumferential Crack ($\alpha = 20^\circ$) in a Cylinder under Tension

$3 \leq R/t \leq 100$

$\frac{1.818c}{\sqrt{R_i t}} \leq 9.6$

Figure 4.15: The Geometry Associated with the API RP 579 Equations for a Through-Wall, Circumferential Crack in a Cylinder
Figure 4.16: Comparison of Deepest Point and Surface Point N-R Flat Plate Solutions for the Circumferentially Welded Tube Tests containing a Surface Flaw at a/t of 0.8 and the Through-Thickness Crack Solutions of Zahoor and API RP 579

Figure 4.17: Comparison of Deepest Point and Surface Point Newman and Raju Flat Plate Solutions for Tube Tests T2 and T4 at a/t of 0.8 and the Through-Thickness Crack Solutions for Tube Tests T1 and T3
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Figure 4.19: Relationship of Experimental Y Factors against c/b for T5
Figure 4.20: Relationship of Experimental Y Factors and Zahoor Solutions against c/b for T5

Figure 4.21: A Central Crack in a Rectangular Sheet under Uniform Uniaxial Tensile Stress (h>>b)
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Figure 4.24: The Typical Stress Distribution for a Tubular Joint Compared with other Simplified Models
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Figure 4.28: Relationship of Experimental Y Factors, Zahoor Solutions, and $Y_T$ against $c/b$ for T6
Figure 4.29: Relationship of Experimental \( Y \) Factors, Zahoor Solutions, and \( Y_T \) against \( c/b \) for T1

Figure 4.30: Relationship of Experimental \( Y \) Factors, Zahoor Solutions, and \( Y_T \) against \( c/b \) for T4
Figure 4.31: Relationship of Experimental Y Factors, Zahoor Solutions, and $Y_T$ against c/b for T2

Figure 4.32: Relationship of Experimental Y Factors, Zahoor Solutions, and $Y_T$ against c/b for T5

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Figure 4.33: Relationship of Experimental Y Factors, Zahoor Solutions, and $Y_T$ against $c/b$ for T3

Figure 4.34: FAD Representing the Initial Tearing Load for the Circumferentially Welded Tubes
Figure 4.35: FAD for the Initial Tearing Load for Tubular T-Joints
CHAPTER 5

5.0 EFFECT OF REDUCTION IN MEMBER CAPACITY ON A STRUCTURE

5.1 INTRODUCTION

It is well established that most structures can withstand a certain amount of member cracking depending on the design philosophy employed. Factors such as the degree of redundancy in the system and multiple load paths are of great importance in providing an extra margin of safety for the designers.

The impetus for this part of the study is to better understand the effects of a large defect on a structure. Knowledge obtained from the previous chapters is used to allow an insight into the local and global repercussions of reduction in the capacity of a member. The importance of structural redundancy and multiple load paths are stressed. The inspection techniques most commonly used to detect large cracking in offshore structures are analysed and the limited repair options are reviewed.

5.2 HISTORICAL OVERVIEW

Structural disasters throughout the latter part of the last century have acted as harsh lessons for the structural engineering community. The consequences of structural failure can not only be measured by the loss of life, but also by the enormous costs and damaged reputations. This section depicts two high profile structural disasters, the space frame supported roof of the Hartford Civic Center and the Alexander Kielland offshore platform. Both catastrophes are chosen as not only they are good
examples of the effect of member failure on a structure, but they also have distinct features that indicate lack of redundancy.

5.2.1 Hartford Civic Center [5.1, 5.2]

The space frame is one of the modern roofing innovations used to enclose large spaces. It is currently being considered for the soon to be rebuilt Wembley Stadium, shown in Figure 5.1 [5.3]. The basic components of a space frame are parallel bars connected by diagonal struts, effectively making a series of triangles. Space frames are light, aesthetically pleasing to the eye and more importantly, economical.

The design of the Hartford Civic Center differed from the standard space frame roof design in several ways due to "money-saving innovations". These are listed in Reference 5.2. There was no previous precedent for these design changes and no suitable physical models were used. The engineers employed a state-of-the-art structural analysis package to verify the safety of the roof.

On the 18th of January 1978 the 2.5 acre roof collapsed in the centre, sending 1400 tonnes of steel, insulation material, and roofing panels into a space where thousands of supporters were watching a sporting event the night before. The official cause of failure was overloading of the members due to heavy rain and snowfall.

A computer modelling package was used to investigate, and simulate, the failure. There were two major findings. It was discovered that the "money-saving" roof design was extremely susceptible to buckling, which was a mode of failure not considered during the designing of the roof. The other revelation was that when one member buckled, the load was transferred to the adjacent members, which were not capable of sustaining the addition and thus themselves buckled. This was a progressive collapse due to the lack of redundancy of the structure.

There were two major lessons learnt by the collapse of the roof of the Civic Center. Firstly, the engineers depended on computer analysis to assess the safety of their
money-saving design. Structural analysis computer packages are, however, only as good as the figures and assumptions used by the programmer to create a model. The analysis offered the engineers a false sense of security. The other lesson emphasised the importance of redundancy within a structure.

5.2.2 The Alexander L. Kielland [5.1, 5.4]

The Alexander Kielland was a semi-submersible platform with a pentagonal type leg design commissioned in 1976. It housed more than 200 workers in the Edda oil field, about 200 miles off the Norwegian coast. It lost a leg and capsized in 1980 during a severe storm, causing the death of 123 workers. The accident was attributed to fatigue crack growth at the attachment for a hydrophone on a 2.4 m diameter horizontal bracing member. The hydrophone was supported by a steel tube, welded into a hole cut into the brace. A fatigue crack had spread from the attaching weld around about one third of the circumference of the brace before fast fracture occurred. Failure analysis investigators discovered that the crack was formed during the incomplete cooling down process of the weld, i.e. the defect had its origins in the yard. Regular inspections (both onshore and offshore) had not detected the crack as it was situated in an “unsuspected” place.

The severance of the cracked brace resulted in overloading of the other five braces securing the leg to the platform. The designers had not contemplated this condition and thus had not allowed for a sufficient degree of redundancy or multiple failure paths. This resulted in the detachment of the remaining braces and subsequently, the leg.

The lack of redundancy was more apparent when considering the stability of the platform. The stability of the structure with a missing leg was not part of the design safety analysis.
5.3 STRUCTURAL REDUNDANCY

Offshore structures are often capable of resisting loads well in excess of those calculated by traditional design practices. This can prove to be very practical as operational conditions are likely to differ from what was assumed at the design stage: operational loads may increase, additional risers may be installed, and the structure will undergo various types of wear and damage. Offshore structures usually have a certain degree of redundancy and total collapse of the structure will not take place before a sequence of failures has occurred.

Redundant members carry negligible load under elastic loading and provide a significant contribution into maintaining the integrity of a structure with damaged members. For example, in the event of damage to diagonal braces, horizontal members would be essential for providing an alternative load path through the structure. This is what the Civic Center in Hartford was lacking.

Additionally, redundancy is an important concept when considering either a *fail-safe* design or *controlled failure design*. The former is dependent on load redistribution amongst adjacent members, while the latter can be accommodated by controlling the failure paths.

The next section is aimed at providing an insight into the methods by which most defects are discovered.

5.4 INSPECTION

Various inspection techniques are used during the fabrication and operation of an offshore platform. Standard techniques such as ultrasonics, magnetic particle inspection, and radiography are used in fabrication yards [5.5]. Sub-sea detection and sizing methods used during platform operation include various forms of visual
inspections, and, where more detailed information on joint integrity is required, sophisticated NDT methods such as Alternating Current Field Measurement (ACFM) or Flooded Member Detection (FMD) are applied.

It has been reported that General Visual Inspection (GVI) and FMD have accounted for the detection of the highest number of significant defects [5.6, 5.7].

The use of the FMD technique has many recognised advantages such as being relatively less expensive than traditional NDT methods, the relative ease of data interpretation and minimal requirement for surface preparation of the member. However, its use as an inspection tool has several limitations. False readings may be obtained if the member is partially filled or where there is debris present in the member. Also, once the flooded member is discovered, other methods, such as MPI or Close Visual Inspection (CVI) are usually needed to detect the exact location of the damage in order to be able to implement a repair option.

Perhaps the biggest perceived disadvantages are the fact that the through-thickness cracks may have grown to a size where the remaining fatigue life of the component is significantly hindered and also, depending on the extent of the cracking and the type of loading, the static strength of the component is severely reduced. These disadvantages are enhanced by the fact that cost saving measures, design improvements, and superior material properties have resulted in lower degrees of redundancy for modern platforms. The findings of this study are related to these disadvantages and are discussed in the following section by analysing the effects of large defects at component level, and subsequently, at a global level.

5.5 LOCAL EFFECTS

Figure 4.32 illustrates the relationship of the Y factor with increase in crack length using experimental values (until the crack had penetrated the chord wall) and the
analytical solution (for a through-thickness crack) discussed in the previous chapter, for test T5. The analytical Y factor solution follows on from the experimental data. It can also be seen that the Y factor is approximately constant from a c/b of 0.3 (when the crack is still partial-thickness) to a c/b of about 0.8. Thereafter, there is a sharp rise in the gradient of the curve indicating a rapid increase in the stress intensity factor and thus a rapid increase in crack growth. From this curve, it can be concluded that the penetration of the chord wall does not necessarily result in a sudden increase in crack growth rate.

Table 2.16 shows the load required for the initial tearing of the existing cracks in the nine tubular joints, the failure load reached, and the percentage cracked area of each joint. Experimental initial tearing loads for the uncracked specimens were not available. The initial tearing load and the failure load were calculated for an uncracked T-joint using the available literature [5.8, 5.9] and were found to be 2480 kN and 3640 kN, respectively. Test T1 contained a cracked area of 16.3%, consisting of a surface crack length of 220 mm and an inner crack length of 100 mm. Upon loading in the purpose built rig, the initial tearing load was recorded at 2100 kN, or about 85% of the calculated load. The maximum load attained during testing of T1 was 2977 kN, or approximately 82% of the estimated load for an uncracked specimen. This indicates that a member containing a through-thickness crack can maintain its strength to a large extent. It should, however, be noted that the advantage of the high residual strength for tubulars containing a through-thickness crack is somewhat countered by the possibility of brittle fracture for members subjected to relatively high levels of cathodic protection (lower voltage potential). Under these conditions, there is little warning prior to catastrophic failure and the load from the affected member cannot be fully redistributed.

It should be noted that the tests were performed in the laboratory, under controlled conditions where great care was taken to ensure a singular loading mode. This will not necessarily be the case in practice as the effect of the environment and the multitude of loading modes will adversely affect the residual strength of the
component. Furthermore, the tubular specimens tested were purposely manufactured for this study and were made from SE 702, which is a relatively new, high ductility, high strength steel. An independent study at Cranfield University (reviewed in Reference 5.10) concluded that the steel had “a uniform clean fine grained microstructure and a good combination of mechanical properties in terms of high strength, good ductility and excellent low temperature toughness”. It should also be considered that the equations used for the calculation of the uncracked strength are based on data from available tubular joints manufactured from typical offshore steels. To avoid erroneous values, the database needs to be updated regularly to accommodate the rapid use of high strength steel in the offshore industry.

An in-service example of the local effects on an offshore frame structure is shown in Figure 5.2 which illustrates a sub-sea section of a North Sea platform [5.11]. ACFM was deployed on all 14 welded connections (W1-W14). A surface crack of 30.2 mm in length and 0.7 mm in depth was discovered at one of the saddle points of W11, connecting CB2 to CB3.

Had the crack not been detected, it could have grown from the N1 criterion to become a through-thickness crack (N3). Thereafter, the remaining fatigue life would be limited and dependent on the degree of redundancy in the structure. Also, as it was shown in the full-scale testing programme reported in Chapter 2, a large through-thickness crack, such as those present in specimens T6 and Y1, will significantly reduce the static strength of the tubular joint.

Further growth of the crack (from N3 to N4) may involve crack branching and deviation from the weld toe. This leads to a large separation of the crack faces and a clear visual indication of joint distress.

Total failure of the joint (the N4 stage) will occur when the load bearing capacity of the remaining ligament is insufficient for the applied load. Generally, offshore
platforms have a multiplicity of load paths. This indicates that the failure of one component does not lead to catastrophic structural collapse. The consequences of load redistribution can be significant since the increase in load on the surrounding members will reduce their remaining fatigue life.

The effect of load redistribution and reduction of member capacity on a global scale is discussed in the following section.

5.6 GLOBAL EFFECTS

From a global perspective, as the crack penetrates the chord wall there will be a certain amount of load redistribution depending on the degree of redundancy of the structure. While this will hinder the crack growth rate of the original member, the surrounding members might experience additional loading or possibly additional loading modes.

The bending moment and the membrane stresses driving the crack growth depend on the cracked section stiffness, and as a consequence, are dependent on the crack size. The cracked section relays lower local loads than the uncracked section due to decreasing stiffness and loss of material. This results in a load shedding effect, [5.12] providing a redundant load path exists, such that the load in the cracked segment is transferred to the unbroken ligament. In the case of Joint W11 in Figure 5.2, the presence of the crack will reduce the bending moment in member CB3 and the extra load is taken up by redundant members, B1 and B2. If continuous loading persists on the damaged structure, the ultimate capacity is reached, further load shedding occurs, and the maximum sustainable load is reduced. This stage, indicated by point D, is displayed on Figure 5.3, which illustrates the loading of a frame from both an intact and a damaged state. The figure also defines the terms residual strength and reserve strength for a structure. It is important that the distinction between these two concepts be clearly identified.
5.6.1 Residual Strength

The ability of a damaged structure to remain intact and redistribute the loads safely without catastrophic collapse is referred to as the residual strength of the structure. Consider a structure in which the load has been redistributed, i.e., point D. When the load is removed, the damaged structure will return to a new, deformed position (indicated by point E). Upon additional load, the damaged structure will distort more easily until its residual strength is reached (point D'). The HSE report OTO 1999 081 [5.13] defines residual strength in terms of a Residual Resistance Factor, RIF, given by:

$$\text{RIF} = \frac{\text{Ultimate Strength of Damaged Structure}}{\text{Ultimate Strength of Undamaged Structure}}$$  \hspace{1cm} (5.1)

The RIF is represented in Figure 5.3 by the ratio of the load at D' to the load at point C.

5.6.2 Reserve Strength

The reserve strength is defined in the HSE report [5.13] as “the ability of the structure to sustain loads in excess of the design value” and is quantified by the use of the Reserve Strength Ratio (RSR), as shown by Equation (5.2).

$$\text{RSR} = \frac{\text{Ultimate Platform Performance}}{\text{Design Load}}$$  \hspace{1cm} (5.2)

This relationship is represented in Figure 5.3 by the ratio of the load at point C to the load at point A. Typical RSR for structures is between 2 to 4 [5.14].

Reserve strength is a fairly basic, highly utilised concept in structural design (more specifically fail-safe design) both at component level and at system level. The failure of one component generally does not limit the capacity of the structure as a
whole, provided there are sufficient redundant members so that the load can be redistributed.

Adding redundant members and increasing the critical failure path creates additional costs at the time of manufacturing, as well as increase in the platform weight. This may be detrimental in both the installation process of the platform as well as the decommissioning. However, it may be reasonably argued that the gained ability of the structure to tolerate damage to some components in service without substantial loss of structural integrity is of greater interest. An increase in reserve strength could also allow for longer inspection intervals.

The loss of the structural integrity of a component from a Jack-up platform has more severe effects than that of a fixed structure as the level of redundancy is lower in the former due to weight restrictions.

This reasoning can be further explained by using the risk analogy. Risk is defined by the Institution of Mechanical Engineers as the “combination of the probability of occurrence of harm and the severity of that harm” [5.15]. This can be represented more simply as:

\[
\text{Risk} = \text{Probability Of Failure} \times \text{Consequence}
\]

(5.3)

Therefore, assuming the probability of member failure in a fixed jacket structure and a Jack-up platform to be equal, the risk factor is greater for the latter due to the lower degree of redundancy and thus the higher the consequence of failure.

The following section briefly depicts a method used to develop a better understanding of the behaviour of the whole structure from both the intact and the damaged state to allow for the optimisation of designs.
5.6.3 Pushover Analysis

Many ageing platforms operate in the more mature offshore oil fields. Often, service requirements for these platforms have changed to accommodate modifications to support more wells, heavier equipment and bridges from neighbouring fields. Certification of the structural capacity of the platform under the increased loading can be accomplished by performing a pushover analysis. This type of analysis is aimed at understanding and predicting the physical behaviour of the structure. Whereas conventional designs mainly focus on the first member failure, global non-linear pushover analysis also accounts for possible redistribution of forces and subsequent member failure, until total collapse. The loading is provided by the incremental addition of wind, wave and operational forces up to a point where the capacity of the system is reached.

Global non-linear pushover analysis is outside the scope of this study.

5.7 REPAIR

The capability to perform adequate repairs is an integral part of an Inspection, Repair, Maintenance programme. Repairs, especially in deep water, can be costly due to the manpower and equipment and the possible downtime. Current available methods include, crack removal, rewelding, member strengthening or complete replacement of the structural component.

For a through-thickness crack, the repair options are somewhat limited. Crack removal procedures such as electrochemical machining and weld toe grinding are not practical. It is therefore stressed that, through regular inspection, wherever possible, a defect should not be allowed to propagate through the component thickness.

Several repair methods have been used successfully on offshore structures containing through-thickness cracks and are listed below:
Mechanical and grouted clamping
Internal grouting of members
Welding (above water)
Hyperbaric welding (under water)
Crack arrester holes
Member replacement

All six procedures were discussed in Chapter 1 as part of the review into the available repair methods.

Sharp et al. [5.5] stated that grouted and mechanical clamps have most frequently been used to repair significant fatigue cracks in nodal joints.

The time taken to correctly size, manufacture and attach a clamp is quite substantial and can take several months. There is also a requirement to wait for a suitable weather window. During the waiting period, the through-thickness crack may continue to propagate and thus the component's remaining life may be compromised.

It is recommended that low cost, temporary repair techniques such as partial removal and rewelding (for above water), and drilling of crack arrester holes (for under water) be used as temporary measures until the clamps are ready to be fitted. Neither rewelding or crack arrester hole drilling will have any bearing on the way the clamps are designed and implemented. Continuous inspection and monitoring of the defect region and the clamps is then required to ensure a successful repair.

5.8 CONCLUSIONS

The local and global repercussions of reduction in the capacity of a member have been reviewed. The importance of structural redundancy and multiple load paths
have been stressed. The inspection techniques most commonly used to detect large cracking in offshore structures have been analysed.

Based on the findings from the previous chapters regarding cracked tubular members, especially those manufactured from high strength steels, it is recommended that FMD might be considered at the design stages of fixed jacket structures, which generally have a relatively large degree of redundancy, and in areas where regular inspection technique deployment is not easily achieved. In the case of a Jack-up, FMD can be used on non-critical members, while more active inspection techniques should be employed for periodic inspection to account for the higher risk factor. The importance of detecting a defect at the earliest possible phase in order to avoid a large, and potentially through-thickness, defect is stressed.

It is important to note that the above comments are made without fully considering the ease or expense of repair once cracks are allowed to propagate through the thickness. This issue is outside the scope of this thesis. A better insight into the topic can be gained from the work of Rodriguez-Sanchez [5.16].
5.9 REFERENCES


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Model for Fracture Mechanics Analysis of Fatigue Cracks in Tubular


5.10 FIGURES

Figure 5.1: The Proposed New Wembley Stadium from Above [5.3]

Figure 5.2: A Sub-Sea Section of a North Sea Platform [5.11]
Figure 5.3: Illustrating the Loading of a Frame from an Intact and a Damaged State [5.13]
CHAPTER 6

6.0 SUMMARY, CONCLUSIONS, AND RECOMMENDATIONS

The main aim of this study was to better understand the behaviour of offshore structures containing large defects. The increasing trend of the offshore industry towards flooded member techniques for the detection of through-thickness cracks was discussed in Chapter 1. This has resulted in concerns regarding the remaining strength of the members containing through-thickness cracks. A method to estimate the residual strength of cracked members can be a very useful tool for the designers. It could allow for the concept of controlled failure design [6.1] to be deployed on certain sections of a structure.

A simple procedure, recommended by BS 7910 [6.2], to conservatively estimate the residual capacity of a cracked tubular joint was reviewed in Chapter 1. It was noted that the existing data used to verify the procedure were mainly from small-scale tests and finite element models. Furthermore, the technique had not been utilised to estimate the residual static strength of tubular joints manufactured from high strength steels. These type of steels, in welded form, may not perform as well as BS 7191 355D steel and could, under certain circumstances, fail in a brittle manner. It was concluded that there was a need for a series of full-scale residual strength tests on pre-cracked tubular joints manufactured from high strength steels.

Two full-scale testing rigs, capable of applying a load equivalent to the plastic collapse capacity of uncracked specimens, were designed and constructed. The results of the six tubular T-joints and the three tubular Y-joints manufactured from SE 702 allowed the following conclusions to be drawn:
• Most specimens failed in a ductile manner. The brittle behaviour of the Y4 specimen was attributed to the high CP potential levels used during the environmental fatigue testing performed in a previous study.

• The capacity of the high strength steel tubular joints was not severely hampered by relatively large cracked sections. As an example, Specimen T1 contained a cracked area of 16.3%, consisting of a surface crack length of 220 mm and an inner crack length of 100 mm. Upon loading in the purpose built rig, the initial tearing load was recorded at 2100 kN, or about 85% of the calculated load. The maximum load attained during testing of T1 was 2977 kN, or approximately 82% of the estimated load for an uncracked specimen.

• Normalised ultimate static strength results were compared with typical data produced from various tests. It was found that the mean reduction in the static strength of cracked high strength steel joints relative to the uncracked strength was lower than that of the available data by 7% at the ordinate intercept. The mean line for the high strength steel data was, however, greater than the recommended BS 7910 procedure for estimating the residual strength. These results indicate that tubular joints manufactured from high strength steels do not suffer from a substantial lower cracked capacity.

A set of tests on full-scale pre-cracked tubes manufactured from BS 7191 355D steel was also completed. The results of the two partial-thickness cracked and the two through-thickness cracked tests showed that there is little difference between the two types of defects of the same length. Moreover, the two specimens with the largest cracks displayed a distinctive second peak, even after extensive crack opening and tearing. Furthermore, Specimen 2 reached its peak capacity after the partial-thickness crack had ‘popped in’. The results suggested that tubes containing large, through-thickness cracks are able to withstand a considerable percentage of their maximum capacity.
The predicted failure loads for through-thickness cracks, using R6-CODE, were considerably lower than the measured failure loads. The predicted loads for surface cracks were relatively close to, but higher than the measured failure loads. This could be due to the fact that there are a greater number of reliable SIF solutions for partial-thickness flaws in cylinders. While the inaccuracy of the specimens containing through-thickness flaws is greater, caution should be adhered when utilising the R6-CODE as a guide to assessing a component with partial-thickness defects as these estimations are non-conservative.

A novel digital photogrammetric method was introduced to maximise the data capture during the testing of the T-joints and the circumferentially welded tubes. The technique allowed for real-time three-dimensional measurements of the deformation in the vicinity of the brace-chord intersection. Continuous improvements to the technique may allow monitoring the deformation of a component from great distances. Moreover, the accuracy of the technique can be improved so that a displacement of a few micrometers can be recorded. It would be of interest to examine the limitations of the method as in the future it could be used to complement, or even replace, strain gauges.

The results of the three sets of tests indicate that there is not a great reduction in strength of a component containing a through-thickness defect. Although, the number of tests performed is relatively small, a valid argument can be presented to justify the use of flooded member detection for monitoring offshore tubular components, in particular those manufactured from high strength steels.

The BS 7910 procedure for the prediction of the residual strength of tubular joints allows for only the comparison of plastic collapse qualities. The Failure Assessment Diagram procedure accounts for both, plastic collapse and fracture. The fracture axis of a FAD is dependent on knowledge of the stress intensity factor for a particular geometry.
There is a lack of stress intensity factor solutions for tubular joints, principally those containing through-thickness cracks. Furthermore, available standards do not lend themselves well to defining a reliable SIF solution for tubes containing large through-thickness defects.

A review of several existing SIF solutions for tubular sections was presented in Chapter 4. The most suitable techniques for both, partial-thickness and through-thickness cracks were selected. The chosen technique was then validated by performing a finite element based study.

A new SIF solution for tubular T-joints, containing through-thickness cracks, under axial loading was presented. The method is based on the SIF at the crack tip and the non-uniform stress distribution present in an axially loaded tubular T-joint. The final solution was compared with the SIF data obtained, from a previous study on the same specimens, while the defects had not yet penetrated the chord wall. The two sets of data showed a good correlation.

The stress intensity factor solutions and results from the tests reported in Chapter 2 and Chapter 3 were used to evaluate the FAD parameters. All tubular components were analysed on the appropriate FAD. The unavailability of experimental fracture toughness values for BS 7191 355D steel may have caused inaccuracies when analysing the circumferentially welded tubes. The relatively high $K_t$ values obtained when assessing the tubular joints indicated that the steel has a good resistance to fracture in welded form. The large $L_t$ values could be due to the conservatism of the equations used to evaluate the uncracked strength. It would be of great interest to perform full-scale static strength tests on uncracked specimens in order to evaluate this presumed conservatism and possibly amend the FAD.

The knowledge gained in the previous chapters was used in Chapter 5 to analyse the local and global repercussions of reduction in the capacity of a member in an offshore structure. The importance of structural redundancy and multiple load
paths were stressed. The inspection techniques most commonly used to detect large cracking in offshore structures were analysed.

Based on the findings from the previous chapters regarding cracked tubular members, especially those manufactured from high strength steels, it was recommended that FMD could be considered at the design stages of fixed jacket structures, i.e. the use of the design for maintenance philosophy, and in areas where regular inspection technique deployment is not easily achieved. In the case of a Jack-up, FMD can be used on non-critical members, while more active inspection techniques should be employed for periodic inspection to account for the higher risk factor. The importance of detecting a defect at the earliest possible phase in order to avoid a large, and potentially through-thickness, defect is stressed.

It is important to note that the above comments are made without considering the ease or expense of repair once cracks are allowed to propagate through the thickness. This issue is outside the scope of this thesis. A better insight into the topic can be gained from the work of Rodriguez-Sanchez [6.3].

It should be stressed that although a procedure for measuring the residual strength of tubular members, particularly those manufactured from a high strength steel, has been successfully completed, and that the results are favourable, the finding in this study cannot necessarily be applied to other types of tubular joints. Two earlier studies had been conducted to provide experimental data considering the fatigue performance of SE 702 under constant amplitude and variable amplitude loading in both air and seawater environments [6.4, 6.5]. The results indicated that the fatigue resistance of SE 702 was not inferior to that of a conventional, medium strength steel. It is recommended that all new materials used to manufacture tubular joints be put through the rigorous testing programme developed in the three studies, in order to justify their use in the construction of an offshore structure.
6.1 REFERENCES


APPENDIX A

APPENDIX A.0: Equations for the Ultimate Capacity of Tubular Joints

A.1: Pan, Plummer, and Kuang Equations [1.33]

Ultimate strength formulae and validity ranges for tubular joints:

**T- or Y-joints - Axial Tension Loading Applied to the Brace**

\[
P_u = 11.5\sigma_y T^2 \sqrt{D/T} (d/D) \sin \theta
\]

where

- \( P_u \) = Ultimate Capacity
- \( \sigma_y \) = Yield Strength

All other symbols are defined in Table 1.1

\[
19 \leq D/T \leq 93
\]

\[
0.19 \leq d/D \leq 1
\]

\[
30^\circ \leq \theta \leq 90^\circ
\]

**T- or Y-joints - Axial Compression Loading Applied to the Brace**

\[
P_u = 3.1\sigma_y T^2 \sqrt{D/T} (d/D) \sin \theta
\]

where

- \( P_u \) = Ultimate Capacity
- \( \sigma_y \) = Yield Strength

All other symbols are defined in Table 1.1

\[
19 \leq D/T \leq 93
\]

\[
0.19 \leq d/D \leq 1
\]

\[
30^\circ \leq \theta \leq 90^\circ
\]
X-joints - Axial Tension Loading Applied to Both Braces

\[
P_u = 22.75\sigma_y T^2 (d/D)^{0.64}/\sin \theta
\]

19 ≤ D/T ≤ 93
0.19 ≤ d/D ≤ 0.8
30° ≤ θ ≤ 90°

and

\[
P_u = 41.5\sigma_y T^2 (d/D)^{3.42}/\sin \theta
\]

19 ≤ D/T ≤ 93
0.8 ≤ d/D ≤ 1.0
30° ≤ θ ≤ 90°

where

\[
\begin{align*}
P_u & = \text{Ultimate Capacity} \\
\sigma_y & = \text{Yield Strength}
\end{align*}
\]

All other symbols are defined in Table 1.1

X-joints - Axial Compression Loading Applied to Both Braces

\[
P_u = 16.31\sigma_y T^2 (d/D)^{0.64}/\sin \theta
\]

19 ≤ D/T ≤ 93
0.19 ≤ d/D ≤ 0.8
30° ≤ θ ≤ 90°

and

\[
P_u = 30\sigma_y T^2 (d/D)^{64}/\sin \theta
\]

19 ≤ D/T ≤ 93
0.8 ≤ d/D ≤ 1.0
30° ≤ θ ≤ 90°

where

\[
\begin{align*}
P_u & = \text{Ultimate Capacity} \\
\sigma_y & = \text{Yield Strength}
\end{align*}
\]

All other symbols are defined in Table 1.1

Ultimate capacity formulae for simple tubular joints:

**Allowable Joint Capacities for T, Y, K, and X-Joints**

\[
P_a = \frac{F_{ye} T^2}{1.7 \sin \theta} Q_u Q_f
\]

\[
M_a = \frac{F_{ye} T^2}{1.7 \sin \theta} (0.8d) Q_u Q_f
\]

where

- \( P_a \) = Allowable capacity for brace axial load
- \( M_a \) = Allowable capacity for brace bending moment
- \( F_{ye} \) = Yield strength of the chord member (or 2/3 of the tensile strength, if less)
- \( Q_u \) = Ultimate strength factor (defined in the reference)
- \( Q_f \) = Nominal longitudinal stress factor (defined in the reference)

All other symbols are defined in Table 1.1
A.3: API RP 2A Equations – Load and Resistance Factor Design [1.36]

Ultimate capacity formulae for simple tubular joints:

**Allowable Joint Capacities for T, Y, K, and X-Joints**

\[
P_a = \frac{F_{ye} T^2}{\sin \theta} Q_u Q_f
\]

\[
M_a = \frac{F_{ye} T^2}{\sin \theta} (0.8d) Q_u Q_f
\]

where

- \( P_a \) = Allowable capacity for brace axial load
- \( M_a \) = Allowable capacity for brace bending moment
- \( F_{ye} \) = Yield strength of the chord member (or 2/3 of the tensile strength, if less)
- \( Q_u \) = Ultimate strength factor (defined in the reference)
- \( Q_f \) = Nominal longitudinal stress factor (defined in the reference)

All other symbols are defined in Table 1.1
A.4: HSE Guidance Notes Equations [1.6]

Characteristic strength of welded tubular joints subjected to unidirectional loading:

*Allowable Joint Capacities for T, Y, K, and X-Joints*

\[
P_k = \frac{F_y T^2 K_a}{\sin \theta} Q_u Q_f
\]

\[
M_{ki}, M_{ko} = \frac{F_y T^2 d}{\sin \theta} Q_u Q_f
\]

where

- \( P_k \) = Characteristic strength for brace axial load
- \( M_{ki} \) = Characteristic strength for brace in-plane moment load
- \( M_{ko} \) = Characteristic strength for brace out-of-plane moment load
- \( F_y \) = Characteristic yield strength of the chord member (or 0.7 times the characteristic tensile strength, if less)
- \( K_a \) = \( \frac{1 + (1/\sin \theta)}{2} \)
- \( Q_u \) = Strength factor (defined in the reference)
- \( Q_f \) = Factor to allow for the presence of axial and moment loads in the chord (defined in the reference)

All other symbols are defined in Table 1.1
APPENDIX B

APPENDIX B.0: CD-ROM Video Footage

A CD-ROM containing video footage of three tubular T-joint tests and the four tests performed on the circumferentially welded brace members can be found in the pocket attached to the inside cover of this thesis. For ease of viewing, clips from each test along with a brief description are presented in a Microsoft PowerPoint document named “Fullscale Videos.ppt”. All required software and linked files are provided on the CD-ROM.
APPENDIX C

APPENDIX C.0: SIF Solutions

C.1: Sanders' Expression for the Dimensionless SIF

Dimensionless Energy Release Rate, $I$, (with respect to half crack angle) from Sanders [4.7]:

$$ I = \alpha^2 \frac{\sqrt{\lambda} g(\alpha) + \pi \lambda^3 C^2 - 2\sqrt{2}}{\varepsilon} $$

where $\alpha = \text{half crack angle}$

$$ \varepsilon = (h/R)[12(1-v^2)]^{1/2} $$

$$ \lambda = \frac{\alpha}{2\varepsilon} $$

$$ C = 1 + \frac{\pi}{16} \lambda^2 - 0.0293 \lambda^3 \quad \lambda \leq 1 $$

$$ C = \left( \frac{2\sqrt{2}}{\pi} \lambda \right)^{0.5} + \left( \frac{0.179}{\lambda} \right)^{0.885} \quad \lambda > 1 $$

$$ g(\alpha) = 2\sqrt{2} \left[ 1 + \frac{1 - \alpha \cot \alpha}{2\alpha \cot \alpha + \sqrt{2 \alpha \cot (\pi - \alpha) / \sqrt{2}}} \right]^2 $$

The dimensionless stress intensity factors for tension case is given by:

$$ F_o = (I/2\pi\alpha)^{1/2} $$
C.2: SIF Expression for a T-Joint Containing a Through-Thickness Defect

Based on Albrecht and Yamada’s non-uniform stress concentration factor [4.19] and Uddin’s three-dimensional finite element model

\[ Y_T = 250.490x^6 - 663.841x^5 + 676.638x^4 - 326.338x^3 + 75.965x^2 - 9.886x + 3.563 \]

where:

- \( Y_T \) = New Y factor for a through-thickness crack in an axially loaded T-joint
- \( x \) = Ratio of half crack length to crack depth \((c/b)\)