Fatigue behaviour of square-to-square hollow section T-joints with corner cracks

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SUMMARY

In this study, the fatigue behaviour of the square-to-square hollow section (SSHS) T-joints with corner cracks is investigated. Both experimental work and numerical modelling and analysis are carried out. Crack profiles in SSHS joints subjected to fatigue loads are studied. A general equation for crack surface in SSHS joints is proposed and a fatigue life prediction procedure is introduced.

In the experimental investigation, fatigue tests are carried out on four SSHS T-joints having different geometrical parameters and subjected to combined fatigue loads. Static tests are first carried out to investigate the Stress Concentration Factor (SCF) and Hot Spot Stress (HSS) distributions along the intersection of chord and brace, and to determine the peak HSS and its location when the joints are subjected to basic and combined load cases. The SCF values are used to validate the existing SCF equations and the superposition method. The peak HSS location is used to determine the placement of the probes. Alternating Current Potential Drop (ACPD) technique is employed to monitor the crack growth and crack shape development on the joints at a pre-set number of cycles. The experimental data are recorded and the fatigue performance of the four T-joints is investigated. The crack growth curves and crack growth rates at the deepest point on each specimen are presented and the crack length versus crack depth is also investigated. The fatigue life results confirm that the S-N design curves can be used safely to predict the total fatigue life of the SSHS T-joints. Based on the test results, it is suggested that the fatigue life of a rectangular hollow section (RHS) joint should be considered as the number of load cycles when the curved crack depth is equal to the chord wall thickness. Crack surfaces and crack shapes are investigated by using a clay modelling technique after the fatigue tests are completed. The crack surface is found twisted and the crack shape is very unsymmetrical.

Based on the information obtained from the fatigue tests, a geometrical mesh model is proposed. The crack surface is represented as an unsymmetrical 3D curved surface with deepest point located at the corner of the brace-chord intersection and the crack shape (front) is modelled as a piecewise unsymmetrical bi-elliptical curve. Subsequently, a mesh generator for SSHS T-joints with surface crack is developed by a tailor-make technique. The entire joint is sub-divided into distinct zones. In each zone, the mesh is generated separately. After the mesh of all the zones are completed, the complete model is obtained by merging the mesh of every zone. The validity of the proposed geometrical model and the reliability of the proposed mesh generation scheme are confirmed by the fatigue test results.

The validated modelling technique is then used to conduct finite element analysis (FEA) on the crack profiles, weld size and contact problems in SSHS T-joints. Based on the FEA results, one general equation to represent the crack surface is proposed. The comparisons between the proposed crack surface and the experimental results showed that the proposed equation is reliable and acceptable for practical application. FEA also showed that in the numerical modelling, both weld size and contact problems should be considered.

Once the accurate stress intensity factor (SIF) is obtained by the proposed numerical modelling, a fatigue life prediction procedure for part-through crack (PTC) is introduced. The procedure is illustrated by using the four test specimens. The validity of the life prediction for PTC is confirmed by comparing against the test results.

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NOTATION

The notation used in the main text is briefly defined in the alphabetical order in the following list. Symbol confined to only a specific analysis is omitted here and is defined at the appropriate point in the text.

$K_A(\phi)$	Stress concentration factors around the intersection of joint subjected to axial loading
$K_{Bi}(\phi)$	Stress concentration factors around the intersection of joint subjected to in-plane bending
$K_{Bo}(\phi)$	Stress concentration factors around the intersection of joint subjected to out-of-plane bending
α	$2L/b_0$, chord length parameter
β	b_1/b_0 , width ratio
Δ_c	Cross-crack probe spacing
Δ_R	Reference-crack probe spacing
δ	Skin depth
γ	b_0/t_0 , chord width to thickness ratio
θ	Brace angle measured from chord
σ_{HSS}, HSS	Hot spot stress
τ	t_1/t_0 , wall thickness ratio
ν	Poisson's ratio
ε_n	Nominal strain
$\varepsilon_{\perp}$	The strain at the weld toe position perpendicular to weld toe
$\varepsilon_{\parallel}$	The strain at the weld toe position parallel to weld toe
a	Projected length of crack surface curve
a'	Curved length of crack surface curve
θ_s	The angle between the crack surface curve and the direction vertical to the chord surface
da'/dN	Crack penetration rate

da/dN	Fatigue crack growth rate (m/cycle)
t_0	Chord thickness
t_l	Brace thickness
ω	Corner angle of square hollow section brace
ϕ	Angle measured around intersection from crown
σ_b	Nominal stresses due to bending loading
σ_m	Nominal stresses due to membrane loading
σ_{max}	Maximum stress
σ_{min}	Minimum stress
σ_n, σ_{nom}	Nominal stress
$a'_{initial}, a'_{final}$	Initial and final curved crack depth
a	Crack depth (surface crack)
ASP	Average stress parameter
AX	Axial loading
b	Plate width
b_0	Outside chord width
b_l	Outside brace width
C, m	Material constant (Paris' constant)
c	Crack half length
CHS	Circular hollow section
d_l	One-dimensional crack depth
E	Young's modulus
f_{ax}	Nominal stresses due to axial loading
f_{ipb}	Nominal stresses due to in-plane bending
f_{opb}	Nominal stresses due to out-of-plane bending
FOS_N	Factor of safety of fatigue life prediction
G	Shear modulus of elasticity
h	Weld height
HSS	Hot spot stress
$HSSN$	Hot spot strain
IPB	In-plane bending

J	J-integral
J_{int}	Interaction J-integral
ΔK	Stress intensity factor range
K_c	The material fracture toughness
K_e	Equivalent stress intensity factor
K_I	Modes I stress intensity factor
K_{II}	Modes II stress intensity factor
K_{III}	Modes III stress intensity factor
ΔK_{th}	Threshold value of stress intensity factor range
l	Crack length
L_{br}	Brace length
L_{ch}	Chord length
M_x	Crack depth modification factor
N, N_f	Number of cycles to failure
N_{I-4}	Four different failure stages
N_r	The residue life of a joint, the number of the load cycles for the crack penetrate from one depth to another one
OPB	Out-of-plane bending
PTC	Part-through crack
Q	Shape factor for semi-elliptic defect
R	Applied stress ratio
r	Distance from crack tip
r_b	Corner radii of the square hollow section brace
r_w	Corner radii of the weld circumference
RHS	Rectangular hollow section
$S, S_{rhs}, \Delta\sigma$	Hot spot stress range
S_B	Stress range at reference thickness, t_B
SCF	Stress concentration factor
SCF_{AX}	Stress concentration factor for AX
SCF_{IPB}	Stress concentration factor for IPB
SCF_{OPB}	Stress concentration factor for OPB
SHS	Square hollow section

$SSHS$	Square-to-square hollow section
SIF	Stress intensity factor
$SNCF$	Strain concentration factor
Snf	Stress-strain conversion factor
t	Reference thickness (16mm for tubular joints)
TTC	Through-thickness crack
u	Displacement in x-direction
v	Displacement in y-direction
V_C	Cross-crack voltage
V_R	Reference-crack voltage
w	Displacement in z-direction
W	Weld thickness
$Y, Y(g), Y_{exp}$	Stress intensity factor calibration

CHAPTER 1

INTRODUCTION

1.1 RESEARCH BACKGROUND

Since the large-scale production of the first rectangular hollow section (RHS) by Stewards and Lloyds in 1952, rectangular hollow sections have been widely used in various structural applications. Compared with other structural sections, RHS has its own merits. RHS can be connected more easily than circular hollow sections (CHS) because the member end preparation requires only straight cuts even for more complex connections. The ratio between cross-sectional area and surface area of RHS is also smaller than that of I-sections, thus protection against corrosion and fire becomes more economical. In addition, by keeping the external dimension constant, the thickness of hollow section can be adjusted to cater to different structural requirements. The same column size can be maintained throughout the full height of the building, which greatly simplifies architectural details and ensuring economy in fabrication. Hence, rectangular hollow section structures are widely used in buildings as well as in topside structures of offshore platforms (Figures 1.1 and 1.2).

Similar to CHS joints, RHS members are jointed together by welding the profiled ends of the braces onto the surface of the chord, which results in rectangular-to-rectangular hollow section (RRHS) joints where large amount of stress concentration occurs due to the structural discontinuity. Under service loading, failures always initiate at this location first. This is why the hollow section joints are potentially the weakest parts in

the structures. Hence, research on these hollow section joints is very important and necessary for the successful applications of these sections.

Over the past 50 years, the analysis and design of RHS joints have received much attention, and many studies have been done. These studies are mainly concentrated on the determination of ultimate static strength, stress concentrations around weld toe, fatigue design life estimation, determination of remaining fatigue life, and repair methods. In the above research areas, partial successes have been achieved because of continuous effort made by researchers. For instance, established and proven equations have been proposed to estimate the stress concentration factor (SCF) on RHS T/X, K (gap), K (overlap) and KK (gap)-joints under axial loading, and T/X-joints under in-plane bending and summarized in CIDECT Design Guide No.8 (Zhao et al, 2000). The S-N (Stress range to Number of Cycle to failure) curves for the estimation of fatigue life are used almost exclusively in design. However, there are still many jobs need to be done. For examples, the equations for SCF under out-of-plane bending are not provided for RHS joints. Stress-life (S-N) curves used currently are proven to be rudimentary and lack consistency (van Wingerde et al, 1995). Few publications on the residual life estimation can be found.

The S-N curves are produced by a series of test database. An S-N curve is a log-log plot of the hot spot stress (HSS) range against the number of cycles to failure (N). The HSS is the peak stress at the intersection of the joint which characterizes fatigue. The advantage of this method is that only one value of stress range, i.e. the HSS range, is used to estimate the fatigue life. This method is easy to use and therefore it is mainly applied in the design stage to predict the total fatigue life of joints. Hitherto, many methods and equations have been proposed to calculate the HSS at the intersection of joints. Effects of various conditions such as corrosion, size effect, stress relief, etc. can also be considered. However, these approaches rely on the accurate prediction of the HSS at joints, and the fatigue life curves are limited to the tested hollow section joint

geometry, configurations and loading conditions because the full-scale test is expensive. Additionally, fatigue life curves can only provide the designer the total fatigue life of a tubular joint, which always includes crack initiation phase, i.e. the time taken to initiate a crack in a joint and crack propagating phase, the time taken for the crack to propagate through the wall thickness. They cannot be used when crack is detected in service in a tubular joint because they do not give any information on the crack propagation phase which accounts for 90% of a joint's life (Haswell and Hopkins 1991).

Fracture mechanics has been proven reliable to evaluate the residual life of joints after cracks are found. It can be very specific to a particular joint configuration and loading. Central to the fracture mechanics method is the stress intensity factor (SIF), which gives the magnitude of the elastic stress field at the tip of an existing crack. However, its application depends largely on the crack information. Many researchers have proposed various models based on the results obtained from full-scale tests or numerical analyses, to determine the SIF of the crack in the CHS joints (Chong Rhee et al, 1991, Cao et al, 1997, Lee et al, 1999 and Chiew et al, 2004). Unfortunately, the crack information in CHS cannot be directly adopted in RHS joints due to geometrical differences. Hence, to understand the behaviour of cracks at the weld toe zone can help to extend the application of the fracture mechanics method in RHS joints. However, this is an extremely difficult task. Firstly, there is great uncertainty about the loading to which a joint within a structure is subjected. Secondly, the geometry of the joint with crack is extremely complex and hence difficult to model. As a result, fatigue behaviour of RHS joints has become a research focus.

Component geometry, crack configuration and the external load determine the stress field at the crack tip, and subsequently the crack growth rate and the crack path during fatigue crack growth (Ling, 1993). Generally, investigation and description of fatigue crack growth include the crack initiation, crack propagation rate in chord thickness direction and crack path on chord surface. At present, a large amount of information on

crack propagation has been published. However, all these information is obtained from both numerical model and experiment on simple component, such as a plate. Even with such these simple structures, the results showed that at the present state-of-art the factors dominating a crack propagating path are not completely understood. In general, crack surfaces are difficult to be predicted, and in practice they are often determined by large-scale structural tests (Pook, 2000). In order to simply the problem, it is usually assumed that the crack surface is known, either from theoretical considerations, or from the results of laboratory tests. Currently, the behaviour of crack surface in RHS joints has not been understood because of the lack of related information.

Because of the high costs of experimental work, the number of the tests carried out in an experimental program is usually limited. The last fifty years has seen a strong growth in the application of numerical methods that offer a cheaper and, if well calibrated, reliable tool to conduct research. Even though many mature techniques for modelling CHS joints with cracks have been reported in literature, few of them can be directly used to model RHS joints with cracks around weld toe. The modelling of the rectangular hollow section joints with arbitrary cracks is a big challenge due to the geometrical complexity of the weld and the crack surface. Although some work were conducted on thin-walled RHS-RHS, CHS-CHS and CHS-RHS T-joints under in-plane bending (Mashiri et al. 2002, 2004a, 2004b; Tong et al 2006).

1.2 OBJECTIVES AND SCOPE

Based on the above discussion, this study will focus on the fatigue behaviour of cracked square-to-square hollow section (SSHS) T-joints. In this study, the SSHS T-joints instead of the more general RHS joints are selected because SSHS T-joint is the most commonly used joint configuration in practice. The main objectives of this study are as follows:

- To investigate the fatigue behaviour of SSHS T-joints under the basic and combined load cases experimentally and to provide basic information about crack propagation in SSHS T-joints, such as crack surface, crack front and crack path around weld toe;
- To develop a mesh generator, which can generate the mesh models of the SSHS T-joints with and without surface cracks at the arbitrary locations along the joint intersections, for numerical analysis of the cracked SSHS T-joints under basic and combined loading;
- To obtain a full understanding of various factors that may affect the crack propagation (Stress Intensity Factor) in chord wall to simplify the finite element modelling in numerical study.
- To propose a general crack surface equation based on the experimental data and numerical analysis that can result in a higher SIF value that leads to conservative life prediction for numerical analysis.
- To propose a life-prediction procedure that can predict the fatigue life (N_f) corresponding to one initial crack size (crack depth $a'_{initial}$) to one final crack size (crack depth a'_{final})

1.3 CONTRIBUTIONS AND ORIGINALITY

The main contributions of the present study are to provide the crack propagation information in SSHS T-joints from experiments and to render an in-depth understanding of the fatigue performance of the SSHS T-joints under various loads from numerical analysis.

The main originality of the work includes the followings:

- a) The experimental investigation on the crack initiation and propagation for large-scale SSHS T-joints;
- b) The experimental investigation on the crack profiles in large-scale SSHS T-joints;
- c) The modelling of the SSHS T-joints with unsymmetrical curved crack(s) located at any position around the weld toe;
- d) The proposing of the crack surface equation and life prediction procedure.

1.4 ORGANIZATION

The contents of this study are arranged as follows:

- Chapter 2 covers literature review. The past research will be reviewed and the background of related theories will be introduced.
- Chapter 3 covers the full-scale static tests. In this chapter, the test rig, specimen details will be introduced. The SCF and HSS distributions around the weld toe will be studied.
- Chapter 4 covers the full-scale fatigue tests. Constant-amplitude fatigue loads will be applied to the specimens. Alternative current potential drop (ACPD) technique will be used to record the crack initiation and propagation. The crack surface will be investigated using a clay modelling process.
- Chapter 5 focuses on the geometrical modelling and mesh generation of SSHS T-joints. In this chapter, a detailed geometrical modelling procedure will be introduced based on the information provided by the large-scale fatigue tests and a tailor-make mesh generator will be proposed. The validity of the modelling technique will be demonstrated against the fatigue test results.

- Chapter 6 covers the finite element (FE) analysis on the crack profiles, weld size and contact problems in SSHS T-joints. Five factors that may affect the stress distribution along the crack front will be analyzed using the modelling technique proposed in above chapter.
- Chapter 7 introduces a procedure for part-through crack (PTC) life prediction. The use of this procedure will be illustrated and its validity will be confirmed by the fatigue test results.
- Chapter 8 summarizes the study and observations in this research procedure. Recommendations will be also given in this chapter for future works.

Figure 1.1 Roof with lattice girders (Hollow sections in structural applications, Bouwen met staal, 2002, p.27)

Figure 1.2 Offshore platforms with jack-up (Hollow sections in structural applications, Bouwen met staal, 2002, p.31)

CHAPTER 2

LITERATURE REVIEW

2.1 JOINT CLASSIFICATION AND GEOMETRICAL PARAMETER

Square-to-square hollow section (SSHS) joints have extensively been used as supporting structures in offshore engineering and onshore structures. Recently, more and more architects have also adopted SSHS joints as part of the supporting systems. A typical SSHS joint consists of one or several smaller square tubes called braces, and one larger square tube called chord. The braces are jointed with the chord by welding the braces onto the outer surface of the chord. The connection between the braces and the chord is called a hollow section joint, which is the most critical part of the entire structural system. The joint intersection is much simple compared with circular-to-circular hollow section (CCHS) joints since it only needs straight cut, which is one of the reasons why square tube is widely used in engineering. These joints can be classified into many types according to their geometries. Using this classification, SSHS joints can be classified as T-, Y-, K-, X-, TT-, XX-, TX- or KK-joints. The configurations of these common hollow section joints are illustrated in Figure 2.1. Although other classifications, such as by the load path, have also been proposed, geometry classification is adopted frequently because it is easier to be understood by designers.

For brevity in design and analysis, the geometry of a SSHS joint can be simplified by expressing it as non-dimensional parameters. Figure 2.2 shows the definition of these parameters which are commonly used in practice. Hence, the characteristics of a T-joint

can be described by some normalized parameters, such as α , β , γ and τ . Some additional parameters such as intersecting angle, θ , and eccentricity, e are used to define Y, K and other type joints. Using this method, the joints having the same parameters of α , β , γ , τ , θ and e will be defined as one numerical model even though they have different diameter, thickness and length.

2.2 STRESS ANALYSIS OF HOLLOW SECTION JOINTS WITHOUT CRACK

It is very important to know accurately the distribution of stress concentration around a welded intersection since it plays a key role in the prediction of fatigue life. In the fatigue design of the hollow section joints, the stress-life (S-N) curve approach is conventionally used. The fatigue life of the hollow section joints can be predicted based on the hot spot stress (HSS) range at the welded intersection. This approach has been used together with parametric stress equations to ensure that the hollow section joints satisfy the fatigue design requirements for many years. On the other hand, when performing remaining life calculations on the cracked joints, fracture mechanics approach should be used. In this approach, the accurate stresses acting on the anticipated crack path must be obtained. Therefore, not only is information required on the absolute maximum value of stress at the intersection, but also the distribution of stress around the intersection as well as the stress variation through the thickness must be available under different type of load conditions.

The maximum stress level around the intersection is traditionally known as the Hot Spot Stress (HSS). The location of this HSS is dependent on the joint shape and loading mode. Under cyclic service loading, crack always initiate at the areas where there are high-level stress concentrations. Therefore, the accurate prediction of HSS and its location becomes the main basis of the fatigue life prediction and the understanding of crack growth behaviour.

For uniplanar SSHS T and X-joint, some interesting locations (A-E) first defined by Puthli et al (1986) are also shown in Figure 2.2.

2.2.1 Stresses in Hollow Section Joints

Stress distributions in welded hollow section joints are very complicated due to the non-uniform stiffness behaviour around the intersection of brace and chord, the structural discontinuities and welding. According to the cause of the stress, the stresses in the intersection of a hollow section joint mainly include:

- a) Nominal stress — the basic stress of the hollow section member due to the applied load. It can be calculated by simple elastic beam theory and superposition method taking into account of any geometric discontinuities and localizing effects. The following equation can be used to calculate this stress,

$$\sigma_n = \frac{F}{A} + \frac{M}{I} \cdot y \quad (2.1)$$

where, F is axial load;

A is area of cross section;

M is bending moment;

I is moment of inertia of the cross section; and

y is distance from neutral axis to the point considered.

- b) Hot spot stress (HSS) — this stress results from the deformation of the chord walls to maintain compatibility at the intersection due to the imposed loads (UEG, 1985). The stress depends only on the global stress concentration and disregarding on the local peak stress due to the weld geometry (Stig et al, 1994).

- c) Local stress or notch stress — this stress is the result of the geometric discontinuity of the hollow section walls due to the weld. The influencing factors include the local notch of the weld toe, the undercut and lack of fusion. This stress may vary widely along the weld and is strongly dependent on the skill of the welder and the welding method. It is not feasible to give explicit correction factors for these influences, so the current practices of demanding a minimum quality of the weld toe would have to be maintained (van Wingerde et al, 1995).
- d) Residual stress — this stress is caused by the weld process due to instant heating and cooling at the joint intersection areas. Most experimental studies towards the hollow section joint stress concentration ignored the effect of residual stress.

2.2.2 Definition of Hot Spot Stress and Stress Concentration Factor

Although HSS is very important in the fatigue assessment of the hollow section joints and is used in many design guidelines, the definition of HSS is still very much under debate. For example, DEn (1984) in its design guidance recommended that hot spot stress “*can be given as the peak value of geometric stress found at the weld toe*”. It “*should incorporate the effects of overall tube geometry (i. e. the relative size of the brace and chord) but omit the concentrating influence of the weld itself which results in a local stress distribution*”. This stress is the maximum principle stress. Based on many research results of past and ongoing research programs, another definition of HSS have also been given by van Wingerde et al (1996) as following:

Hot Spot Stress (σ_{HSS}) is the extrapolated value at the weld toe position of the structural stress perpendicular to the weld toe.

Here this stress is the structural stress perpendicular to the weld toe. This definition of HSS was adopted by the IIW fatigue design guide (IIW, 1999), and will also be adopted in this thesis.

In many design guides, Stress distributions and HSSs are usually defined in terms of stress concentration factors (SCFs) where:

$$\sigma_{HSS} = \sigma_{nom} \times SCF \quad (2.2)$$

σ_{HSS} - Stress at point of interest around intersection

σ_{nom} - Nominal brace response to applied load

The stress concentration factor, namely SCF, is the ratio between the hot spot stress, caused by structural discontinuities or weldment, and the nominal member stress. It is only related to the geometry of the joints. Thus, the hot spot stress calculations can be simplified to determine the SCFs for the certain hollow section joints.

2.2.3 Parametric Equations for predicting SCF of SSHS T-joints

Up to now, much research effort has been made to derive SCF parametric equations for fatigue strength assessment of SSHS T-joints in order to accurately predict the HSS for fatigue design and use it easily. One orientation study was carried out by Puthli et al (1986) who gave five fixed lines as the interesting position of hot spot stress on rectangular hollow section around each corner, as shown in Figure 2.3. The SCF equations of uniplanar T- and X-joints for the five fixed lines are based on the work of van Wingerde (1992), which has been summarized in CIDECT (Zhao et al, 2000). This set of parametric formulae has been used in conjunction with test results to establish $S_{th,s}-N_f$ lines. However, there are only SCF equations under axial load (AX) and in-plane bending (IPB). There is no equation available for out-of-plane bending (OPB). This limits the application of superposition method when the joint is under combined load with OPB component.

Another set of parametric equations was given by Soh and Soh (1992). However, this set of formulae does not provide formulae at fixed locations, just the maxima in chord

and brace. This effectively limits consideration of the superposition of load cases, as also noted by Soh and Soh themselves, who proposed correction factors for combined load cases. In their analysis, facet shell elements were used. The welds of the joint was not simulated in the finite element models. As a result, as Soh and Soh (1990) pointed out that the omission of the welds should give rise to higher stress at brace and chord intersection.

2.2.4 Stress Distribution around the Intersection

In practice, the hollow section joints are usually subjected to combined loadings. So the hot spot stress is a function of all nominal stresses multiplied by their SCFs. In the case of X – and T – connections where only axial forces and in-plane bending moments are considered, the total hot spot stress can be determined by:

$$S_{rh.s} = \sigma_{rm1}SCF_{m1} + \sigma_{ra1}SCF_{a1} + \sigma_{rm0}SCF_{m0} + \sigma_{ra0}SCF_{a0} \quad (2.3)$$

where:

σ_{rm1} = the nominal in-plane bending stress range in the brace,

σ_{ra1} = the nominal axial stress range in the brace,

σ_{rm0} = the nominal in-plane bending stress range in the chord,

σ_{ra0} = the nominal axial stress range in the chord,

SCF_{m1} , SCF_{a1} , SCF_{m0} , and SCF_{a0} are the corresponding SCF.

As the current SCF equations only consider the axial loads and in-plane bending and the SCF formulae for out-of-plane bending are still lacking, the superposition method that was proven in CHS joints will be adopted in this thesis. This method suggested by Gulati et al (1982) is to superpose the stress distributions from each of basic load modes to obtain the HSS around the intersection. The equation proposed by them can be expressed by the following.

$$\sigma(\phi) = K_A(\phi)f_{ax} + K_{Bi}(\phi)f_{ipb} + K_{Bo}(\phi)f_{opb} \quad (2.4)$$

where $K_A(\phi)$, $K_{Bi}(\phi)$ and $K_{Bo}(\phi)$ specify the stress concentration factors around the intersection of joint subjected to AX, IPB and OPB respectively, and f_{ax} , f_{ipb} and f_{opb} are the corresponding nominal stresses.

2.3 FATIGUE DESIGN GUIDANCE FOR HOLLOW SECTION JOINTS

2.3.1 Basic Concept of Fatigue

Fatigue is defined as damage that results in fracture after a sufficient number of stress fluctuations. Service load conditions often involve a load spectrum of different stress ranges and Miner's hypothesis of cumulative damage enables the groups of amplitude to be replaced by an equivalent number of cycles (N) to a single stress magnitude (S) (Rollason, 1973). Thus, the fatigue performance can be characterized as a plot of stress range versus number of cycles to failure. This is the concept of S-N curve. A set of typical S-N curves for steel hollow section joints is presented in Figure 2.4.

In Figure 2.4, hot spot stress range (S_{rhs} , S or $\Delta\sigma$) is the main factor for fatigue analysis, especially for welded hollow section joints. As shown in Figure 2.5, stress range is defined as the value difference between the maximum and the minimum stress ($\Delta\sigma = \sigma_{max} - \sigma_{min}$). Another term relative to this is the stress ratio (R), which is defined as the ratio of minimum stress to maximum stress ($R = \sigma_{min}/\sigma_{max}$) as shown in Figure 2.5. Due to the appearance of residual stresses, the stress ratio, R, is not taken into account in modern fatigue design (IIW 1999).

2.3.2 Definition of Failure for a Cracked Hollow Section Joint

The fatigue life (number of cycles to failure) depends on the failure criteria adopted. There are mainly three failure criteria existing for steel hollow section joints.

One criterion is based on four different failure stages. Each stage has a corresponding life defined as:

- N1 corresponding to a 15% change in strain measured “near” the crack initiation point;
- N2 corresponding to the first “visible” crack;
- N3 corresponding to a through thickness crack;
- N4 corresponding to the end of the test or the complete loss strength.

Another criterion is based on the complete fracture or a certain surface crack length (van Wingerde 1992). The third criterion is the through-thickness crack failure. The third one is as same as N3 in the first criterion, which is currently widely used. N1 and N2 are not widely adopted as failure criteria because these two stages are too conservative for a joint failure. At these two stages, the joints still have enough capability to resist external load and internal pressure. It will also give very different results if different methods are used to monitor N1 and N2. The reason that N4 is not adopted is that a joint under this condition is unlikely to be repaired and may have shed load to other joints causing further remote damage. The second criterion is not widely used because it is difficult to control this standard. The through-thickness crack can be accurately measured using Alternating Current Potential Drop (ACPD) techniques or a pressure drop in internally pressurized members. It is thought to be subjected to small errors than N1, N2 and N4. Hence, through-thickness crack is currently widely adopted as failure criterion.

2.3.3 S-N Curves for Design

The determination of HSS is only one step in determining the fatigue life of a specific connection and load case. An S-N line is used to relate the HSS range to the expected fatigue life of a connection. The S-N curve is simple to use because it just considers the load stress, member thickness and cycle number N . However, the establishment of this S-N curve is expensive and time-consuming because it is derived from a series of experimental tests on representative geometries and materials. Each test can only generate one single data point. At beginning, small-scale hollow section joints and fillet welded plates were tested in air under constant amplitude cyclic loading. Based on these databases, the first S-N curve, namely X_1 -curve, was published by AWS in 1972. Later, an additional, lower curve, X_2 -curve, was proposed by AWS. The X_2 -curve describes the fatigue behaviour of hollow section joints not having a weld profile which “merges smoothly with the adjoining base metal” at the weld toe. The two curves also have been adopted by API (1993) who used the notation X - and X' - instead of AWS X_1 - and X_2 -curve.

In the United Kingdom, Department of Energy (DEn) also published fatigue design guidance in the form of Q-curve. The Q-curve utilizes same database of the results as X-curve. Later, many full-scale ($D > 150$ mm) tests on hollow section joints have been conducted in some test programs such as United Kingdom Offshore Steel Research Programme (UKOSRP) and European Coal and Steel Community (ECSC) programs. The test results revealed that the Q-curve might be lack of conservatism under certain conditions. Hence, in the 1984 edition, DEn code (1984) revised the Q-curve and published another S-N curve, T-curve. T-curve is the first S-N curve based on full-scale test results. Since the inclusion of the T-curve, additional tests have been conducted to assess the validity of design codes. The Department of Energy in 1990 gave another revision of T-curve, which takes into account the most recent test results. Now this revised T-curve is widely adopted in many design codes such as DEn (1990), EC3 (1992), IIW (1999) and CIDECT (Zhao et al, 2000).

In design guidance, a basic S-N curve is generally used for hollow section joints with a certain thickness wall ($t=16\text{mm}$ in DEn). For joints with wall thickness other than this one, thickness correction factors are introduced. For example, in the DEn guidance (1990), for $t>16\text{mm}$, the recommended correction is given by the following equation:

$$S = S_B \left(\frac{t_B}{t} \right)^{0.3} \quad (2.5)$$

where S is the stress range resulting in same fatigue endurance at thickness, S_B is the stress range at reference thickness, t_B is the reference thickness (16 mm for hollow section joints), and t is the plate or joint thickness. For $t<16\text{mm}$, no thickness effect is taken into account due to a lack of reliable data.

Based on the works done by van Wingerde et al (1997, 1998), IIW (1999) guidance had proposed the correction formulae which are listed in Table 2.1. CIDECT (Zhao et al, 2000) had also adopted these correction equations for fatigue design.

2.4 LINEAR ELASTIC FRACTURE MECHANICS

In order to assess the resistance to failure of components that contain initial defects or discontinuities which develop fatigue cracks, the field of fracture mechanics has been developed over the past several decades. Fracture mechanics can be subdivided into two general categories, namely linear-elastic fracture mechanics (LEFM) and elastic-plastic fracture mechanics (EPFM). LEFM is based on a linear-elastic analysis of the stress at the crack tip, and inherently takes no account of plasticity. The presence of a crack in a body represents a stress singularity. This leads to localized inelastic deformation upon loading of the body due to the high stresses at the crack tip. However, if the size of the plastic zone at the crack tip is small relative to local geometry, LEFM can be used without any modification.

Thus an important restriction to the use of EPFM is that *the plastic zone size at the crack tip must be small relative to the geometrical dimensions of the specimen or part* (Naess, 1985). In the majority of fatigue situations cracking will occur under quasi-elastic conditions. In other words, the size of any plastic zone at the crack tip tend to be small compared with the crack length, and thus it is possible and adequate to calculate the conditions around the crack by using LEFM. However, it should be noticed that during early crack growth when the fatigue crack is 'short' in welded hollow section joints, LEFM is invalid and EPFM must be adopted to analyze the crack tip stress (Monahan, 1994). Discussion in this thesis will be limited to LEFM.

2.4.1 Analytical Basis of LEFM

The basis of LEFM is an analysis of the elastic stress field at the tip of a crack. A crack in a solid can be stressed in three different modes, as illustrated in Figure 2.6. The superposition of the three modes describes the general case of cracking. In order to describe the crack modes easily, a coordinate system is introduced in Figure 2.7. Mode I, the opening mode, is characterized by local displacements that are symmetric with respect to the x-y and x-z planes. The two fracture surfaces are displaced perpendicular to each other in opposite directions. Mode II, sliding or shear mode, is associated with local displacements that are symmetric with respect to the x-y plane and skew symmetric with respect to the x-z plane. The two fracture surfaces slide over each in a direction perpendicular to the line of the crack tip. Mode III, the tearing mode, is depicted with local displacements that are skewing symmetric with respect to both x-y and x-z planes. The two fracture surfaces slide over each other in a direction that is parallel to the line of the crack front. For a fatigue crack in a hollow section joint the conditions at the crack tip involve mixed mode loading, but the importance of the opening Mode I generally exceeds the other two modes (Chong Rhee et al, 1991).

The stresses and displacements at any point near the crack tip can be derived, using the theory of elasticity and complex stress functions. Based on Westergaard's complex

stress functions, Irwin (1957) obtained the crack tip solutions given below for each of the three modes of a crack in relation to the coordinate system shown in Figure 2.7.

For mode I:

$$\begin{aligned}\sigma_x &= \frac{K_I}{\sqrt{(2\pi r)}} \cos \frac{\theta_p}{2} \left(1 - \sin \frac{\theta_p}{2} \sin \frac{3\theta_p}{2} \right) \\ \sigma_y &= \frac{K_I}{\sqrt{(2\pi r)}} \cos \frac{\theta_p}{2} \left(1 + \sin \frac{\theta_p}{2} \sin \frac{3\theta_p}{2} \right) \\ \tau_{xy} &= \frac{K_I}{\sqrt{(2\pi r)}} \sin \frac{\theta_p}{2} \cos \frac{\theta_p}{2} \cos \frac{3\theta_p}{2}\end{aligned}\tag{2.6a}$$

For plane strain conditions

$$\begin{aligned}\sigma_z &= \nu(\sigma_x + \sigma_y) \text{ and } \sigma_{xz} = \sigma_{yz} = 0 \\ u &= \frac{K_I}{G} \sqrt{\frac{r}{2\pi}} \cos \left(\frac{\theta_p}{2} \right) \left[1 - 2\nu + \sin^2 \left(\frac{\theta_p}{2} \right) \right] \\ v &= \frac{K_I}{G} \sqrt{\frac{r}{2\pi}} \sin \left(\frac{\theta_p}{2} \right) \left[2 - 2\nu - \cos^2 \left(\frac{\theta_p}{2} \right) \right] \\ w &= 0\end{aligned}\tag{2.6b}$$

For mode II:

$$\begin{aligned}\sigma_x &= \frac{-K_{II}}{\sqrt{(2\pi r)}} \sin \frac{\theta_p}{2} \left(2 + \cos \frac{\theta_p}{2} \cos \frac{3\theta_p}{2} \right) \\ \sigma_y &= \frac{K_{II}}{\sqrt{(2\pi r)}} \sin \frac{\theta_p}{2} \cos \frac{\theta_p}{2} \cos \frac{3\theta_p}{2} \\ \tau_{xy} &= \frac{K_{II}}{\sqrt{(2\pi r)}} \cos \frac{\theta_p}{2} \left(1 - \sin \frac{\theta_p}{2} \sin \frac{3\theta_p}{2} \right)\end{aligned}\tag{2.7a}$$

For plane strain conditions

$$\sigma_z = \nu(\sigma_x + \sigma_y) \text{ and } \sigma_{xz} = \sigma_{yz} = 0$$

$$u = \frac{K_{II}}{G} \sqrt{\frac{r}{2\pi}} \sin\left(\frac{\theta_p}{2}\right) \left[2 - 2\nu + \cos^2\left(\frac{\theta_p}{2}\right) \right] \quad (2.7b)$$

$$v = \frac{K_{II}}{G} \sqrt{\frac{r}{2\pi}} \cos\left(\frac{\theta_p}{2}\right) \left[-1 + 2\nu + \sin^2\left(\frac{\theta_p}{2}\right) \right]$$

$$w = 0$$

For mode III:

$$\tau_{xz} = \frac{-K_{III}}{\sqrt{(2\pi r)}} \sin \frac{\theta_p}{2} \quad (2.8a)$$

$$\tau_{yz} = \frac{K_{III}}{\sqrt{(2\pi r)}} \cos \frac{\theta_p}{2}$$

For plane strain conditions

$$\sigma_x = \sigma_y = \sigma_z = \tau_{xy} = 0$$

$$u = v = 0 \quad (2.8b)$$

$$w = \frac{K_{III}}{G} \sqrt{\frac{r}{2\pi}} \sin\left(\frac{\theta_p}{2}\right)$$

where, r and θ_p are radius and angle that are shown in Figure 2.7; u , v and w are the displacements in the x , y and z directions; G is the shear modulus of elasticity; and ν is Poisson's ratio. The plane strain form of these equations is obtained by substituting ν with $\nu/(1+\nu)$. The parameters K_I , K_{II} and K_{III} , which are the SIFs (Stress Intensity Factors) corresponding to Modes I , II and III , are a measure for the stress singularity at the crack tip. In all cases, the general form of the SIF is given by:

$$K = Y(g)\sigma\sqrt{a} \quad (2.9)$$

where,

$Y(g)$ - parameter that depends on the specimen and crack geometry.

σ - nominal stress applied.

a - crack half length.

2.4.2 Estimation of Crack Growth

It is the objective of fracture mechanics to estimate the total number of cycles for the assumed or the initial crack, a_i , to reach its final length, a_f . The crack tip SIF is an extremely useful parameter to address crack growth behaviour as long as the bulk of the material is elastic and plastic deformation is limited to a small region at the crack tip. The SIF range associated with maximum and minimum stresses of each cycle ($K_{\max} - K_{\min}$) can be used to describe the amount of crack advancement, $\Delta a = a_f - a_i$, during ΔN number of cycles. There are many fatigue crack growth laws that have been developed of varying complexity. And these can be divided in two main categories, i.e. theoretical and semi-empirical laws (Naess, 1985).

Many attempts have been made to deduce a law of fatigue crack growth theoretically, but none of the proposed expressions has a general applicability. So semi-empirical laws are used widely to propose the crack growth relationships. One of the simplest and most widely used models was developed by Paris and Erdogan (1963).

$$\frac{da}{dN} = C(\Delta K)^m \tag{2.10}$$

where,

da/dN	Fatigue crack Growth Rate (m/cycle);
C, m	Material Constants (Paris Constants);
ΔK	Stress Intensity Factor Range.

In general fatigue crack growth can be divided into three distinct regions, A, B and C as shown in Figure 2.8 (Naess, 1985). Region A is a region of very low growth rates. Reducing the value of SIF range in this region leads to a point where crack growth no longer occurs. This is known as the threshold value of SIF range and is denoted by ΔK_{th} . Fatigue crack growth in Region A is heavily influenced by microstructure, mean stress and environment effects. Region B consists of a linear $\text{Log}(\Delta K) - \text{Log}(da/dN)$ relationship. Crack growth rates in this region are relatively insensitive to microstructure, mean stress and environment effects. Region C consists of accelerating, rapid unstable crack growth and is generally dependent on microstructure and mean stress. The Paris law described by Eqn. (2.10) is applicable only to the Region B. It is also observed that it is conservation in region A, and non-conservation in region C (Naess, 1985). Usually, most of the fatigue is to spend propagating cracks in regions A and B. Very little fatigue life is left when a crack enters region C. Thus, the Paris law will in general yield conservative result (Naess, 1985), and used widely.

2.5 FRACTURE MECHANICS MODELS FOR CRACKED HOLLOW SECTION JOINTS

When fatigue cracks are detected during service stage, fracture mechanics assessment of joints has to be performed to predict the residual life. However, because of complex geometric and loading conditions of the hollow section joint with cracks, the accurate fracture mechanics assessment of a cracked hollow section joint is an extremely difficult task. Many attempts have been made to model fatigue performance of cracked hollow section joints using a variety of methods. Haswell & Hopkin (1991) grouped these attempts into four categories, namely:

- a) Semi-Empirical Models—based on full-scale hollow section joint tests
- b) Plate Models—analytical or numerical
- c) Cylindrical Models

- d) Numerical Models—based on stress analysis methods such as those using finite elements.

Haswell & Hopkin (1991) gave a detailed review of some of these models and concluded that the fracture mechanics analysis of cracks in hollow section joints should be treated cautiously because there is no accepted benchmark solution for any type of hollow section joint for comparison. The closest to an accepted solution is the finite element analysis which can accurately model geometry, loading and structural restraint, but this finite element analysis should be validated by the accurate information on crack propagation and shape development obtained from experiment. Some abstract reviews of the fracture mechanics models, which have been proposed, will be presented in the following.

2.5.1 Semi-Empirical Models

Some semi-empirical models have been produced using the SIF from crack growth measurement recorded in full-scale tests. One of these widely used models was developed by Dover and Dharmavasan (1982). This model is also known as Average Stress Model (AVS). In the model, the Paris law was used. And the SIF is generally defined as:

$$K = Y\sigma\sqrt{\pi a} \quad (2.11)$$

where Y is the SIF calibration, which is dependent on joint geometry and loading. The kernel variable of this model is the average stress parameter, ASP (Dover and Dharmavasan, 1982).

$$ASP = SCF_h / SCF_{av} \quad (2.12)$$

where SCF_h is the Hot Spot Stress and SCF_{av} is the average stress around the intersection of joint. SCF_{av} is given by Eqn. (2.13) for in-plane bending and Eqn. (2.14) for out-of-plane bending and axial loading.

$$SCF_{av} = \frac{1}{\pi} \int_{-\pi/2}^{\pi/2} SCF(\phi) d\phi \quad (2.13)$$

$$SCF_{av} = \frac{1}{\pi} \int_0^{\pi} SCF(\phi) d\phi \quad (2.14)$$

where ϕ is the angle around intersection ($\pi/2$ is the saddle position). The Y factor in the semi-empirical model proposed by Dover and Dharmavasan (1982) is given as following:

$$Y = A \left(\frac{T}{a} \right)^j \quad (2.15)$$

where

$$A = 0.73 - 0.18ASP \quad (2.16)$$

$$j = 0.24 + 0.06ASP \quad (2.17)$$

T = chord wall thickness;

a = crack depth.

The AVS model is based on data collected from chord side fatigue cracks in joints covering the parameter range:

$$0.21 \leq \beta \leq 0.76$$

$$2.66 \leq SCF_h \leq 9.4$$

$$1.51 \leq SCF_{av} \leq 6.35$$

$$16mm \leq t \leq 45mm$$

Austin (1994) and Myers (1998) all used the results from full-scale tests to assess this model. It was noted by Austin (1994) that this model tended to over predict the experimental Y factors derived from variable amplitude corrosion fatigue tests on axially loaded T joints. He suggested that the over-prediction should be ascribed to the cycle counting technique. Austin thus proposed a modification to the AVS model which is simply to reduce 15% of the Y factor predicted by the AVS resulting in an improved fit to experimentally derived Y factors. Myers (1998) evaluated the AVS model and the improved one given by Austin in his study. He found that both of the two AVS models, especially the one improved by Austin could provide a good fit to experimentally derived Y factors. Myers also pointed out that the difference between the AVS model and the experimental results of Austin was caused by geometry effect rather than cycle counting method.

Recently, Etube et al (2000) also developed a new method for predicting SIFs in cracked hollow section joints based on the experimental results that were used to produce the AVS model and the results from the modified Newman and Raju flat plate solution (1981). The method has the same form as that of AVS equation. The proposed Y factor solution in this method is given by,

$$Y = A \left(\frac{T}{a} \right)^j - \text{Exp} \left[- \text{Exp} \left(A - \frac{a}{c} \right) \left(\frac{a}{T} \right) \right] \quad (2.18)$$

Where

$$A = 0.56 - 0.18ASP \quad (2.19)$$

$$j = 0.22 + 0.06ASP \quad (2.20)$$

T = chord wall thickness;

a = crack depth;

c = crack half width;

ASP is obtained by Eqn. (2.12).

Etube et al (2000) used this method to predict the SIFs of the Y-joints whose experimental results were used for producing the *AVS* model. They found that the predicted results fit the experimental results better.

The semi-empirical models proposed above were all produced using the data from the full-scale tests. The accuracy of the prediction is also based on the data obtained from tests. For the joints within the validate range of the models, the semi-empirical models seems to be a good approach to predict the SIFs of the cracked hollow section joint but the problem is that the full-scale fatigue tests are time consuming and expensive. It is not easy to get the sufficient fatigue information for different types of hollow section joints with different loading modes. Thus, they should be used with caution when applied to other joint geometry and loading conditions.

2.5.2 Flat Plate Derived Models

Flat plate derived models have been developed to calculate the SIF for semi-elliptical surface cracks in flat plates under tension and bending loads. And it has been proved that these models can be used to analyse cracks in hollow section joints after considering correction factors (Maddox, 1975, Burdekin et al, 1986, Dover and Connolly, 1986, Thorpe, 1986, van Delft et al, 1986 and Bowness and Lee, 1996). The most widely used flat plate derived models are generally based on the equations derived by Newman and Raju (1981) as following:

$$K = \sigma \sqrt{\frac{\pi a}{Q}} M \left(\frac{a}{t}, \frac{a}{c}, \frac{c}{b} \right) \quad (2.21)$$

where σ is nominal stress in plate, Q is shape factor for semi-elliptic defect, M is plain plate geometric factor, a is crack depth, t is plate thickness, c is crack half width and b is plate width.

Thorpe (1986) used the Eqn. (2.18) to predict the SIFs of full-scale hollow section tests by averaging the surface stress at the crack position in the joint over the crack length and simplifying the through-wall stress distribution into membrane and bending components in the following form:

$$K = (M_m \sigma_m + M_b \sigma_b) \sqrt{\frac{\pi a}{Q}} \quad (2.22)$$

This model was evaluated by Haswell and Hopkins (1991) and Myers (1998). All of them found that this model generally over-predicts the SIF for all crack depth. Haswell and Hopkins (1991) pointed out that this problem should be caused by not including the geometry dependent compliance effects in the model.

Some researchers then considered the notch stress component in the Eqn. (2.22) by using the weld toe correction factor, M_k , in the “ M ” term.

$$K = (M_{km} M_m \sigma_m + M_{kb} M_b \sigma_b) \sqrt{\frac{\pi a}{Q}} \quad (2.23)$$

Considerable work has been carried out to determine the M_k for welded structures, for example Burdekin et al (1988) and Bowness and Lee (1996).

Instead of assuming constant stress across the crack surface and a simple crack shape in the early models, Burdekin et al (1986), Dover and Connolly (1986), Nui and Glinka (1989) all used a weight function method to calculate the SIFs of cracked joints. The form of the weight function proposed by Burdekin et al (1986) is as following,

$$W_{qq'} = \frac{K_{q'}}{P_q} \quad (2.24)$$

where $W_{qq'}$ is weight function, $K_{q'}$ is SIF from weight function and P_q is applied load at point q. This method allows for any variation of stress across the crack surface and any variation in crack shape. Haswell and Hopkins (1991) evaluated the model proposed by Burderkin et al (1986) and suggested that the Burdekin et al's (1986) model can be used to calculate the SIF in highly restrained cracked joints.

The flat plate derived models are analytical and easy to apply, but their shortages are also very obvious. The crack propagation behaviour of the fatigue crack in a hollow section joint is known to be significantly different from that of a similar defect in a flat plate because the structural restraint conditions on them are much different. Though many attempts have been done to refine the flat plate models by considering some of these structural restraints found in the hollow section joints, these models still cannot model the major effects such as loading shedding in the hollow section joints.

2.5.3 Numerical Models

Finite element method (FEM) has been widely used to analyse the hollow section joint models. It has been proved to be a reasonable method to analyse hollow section joints fatigue problem because it has the greatest potential to accurately model the geometry, loading and structural restraint of the hollow section joints (Haswell and Hopkins, 1991). There are three types of three-dimension (3D) finite element models that have been proposed for hollow section joints:

- (a) The complete hollow section joint is modelled using shell finite elements, and the crack modelled using line spring elements. The limitation of this model is that it cannot model the weld.

- (b) The complete hollow section joint is modelled using shell finite elements, the crack and weld are modelled using solid 3D elements.
- (c) The complete hollow section joint with crack and weld is modelled using solid 3D elements.

The model (a) was used extensively in the researches such as Huang et al (1988) and Haswell (1991). Bowness and Lee (1995a) assessed this model and found that the accuracy of the line spring element was reasonable only for cracks with moderate depth ($0.35 < a/t < 0.8$). Beyond this range, the SIF solutions were found to be poor. Due to this problem and the fact that the weld toe effects are not well represented, Bowness and Lee (1995a) concluded that this model has limited application in hollow section joints.

In order to accurately model the crack and consider the weld toe effects, the model (b) was developed and was used to analyse the hollow section joint fatigue problem by Du & Hancock (1987) and Chong Rhee et al (1991), etc. The fracture mechanics model provided by Chong Rhee et al (1991) was recommended for inclusion in the revised BS PD6493. The SIF solution of Chong Rhee et al (1991) was based on 40 finite element models of cracked hollow section T joints generated by PRETUBE (1991). In the model, the intersection between the brace and chord was modelled using 20 node isoparametric elements and 8 nodes thick shell elements formed the remainder of the joint. The analysis results of these 40 models were then used to deduce a set of SIF empirical formulae which are in a form similar to those of hollow section joint SCFs. That makes the formulae easy to use and further makes the fracture mechanics fatigue life analysis of the hollow section joints efficient and inexpensive. The SIFs for the deepest point along the crack front for a hollow section T joint under axial loading is given as following:

$$K_e = \sigma_n Y_g Y_i Y_s \sqrt{\pi a} \quad (2.25)$$

where

Y_g is the joint geometry factor;

Y_i is a crack size factor;

Y_s is a joint and crack coupling factor;

σ_n is the brace nominal stress;

K_e is the equivalent SIF, and is defined as Eqn. (2.26)

$$K_e = \sqrt{K_I^2 + K_{II}^2 + \frac{K_{III}^2}{1-\nu}} \quad (2.26)$$

and

$$Y_g = 0.2749\beta^{-0.6225-1.2685\ln\beta}\gamma^{1.3191-0.1661\ln\gamma}\tau^{1.6621+0.3704\ln\beta}$$

$$Y_i = \beta^{0.35621A-0.0956C}\gamma^{0.0983A+0.2298C+0.0817C^2}\tau^{-0.0762A}$$

$$Y_s = (a/t)^p (3c/d)^r$$

$$p = -0.8669 - 0.2198A - 0.0162A^2 - 0.4750C^2 - 0.1667C^3 - 0.0193C^4$$

$$r = 0.0777 + 1.0531A + 0.5820A^2 + 0.0810A^3 - 0.7001C - 0.0604C^2 + 0.0060C^3$$

$$A = \ln\left(\frac{a}{t}\right)$$

$$C = \ln\left(\frac{3c}{d}\right)$$

where

β , γ , τ are the geometric parameters for hollow section joint;

$\frac{a}{t}$, $\frac{3c}{d}$ are parameters for surface crack.

The model is valid over the following range of joint size and crack shape parameters:

$$\alpha = 12$$

$$0.40 \leq \beta \leq 0.80$$

$$10.0 \leq \gamma \leq 20.0$$

$$0.3 \leq \tau \leq 1.0$$

$$0.05 \leq a/t \leq 0.80$$

$$0.5 \leq 3c/d \leq 1.20$$

This solution only considers the situation that the crack is located at the saddle of the hollow section T-joint and the valid range of the joint geometry and crack profiles is limited. That greatly restricts the application of this solution. Another problem of this solution is that Myers (1998) compared the Y factors calculated using this model to the experimental derived Y factors from full scale test in his thesis, and found that the SIFs predicted by this model are over conservative. So Myers concluded that this model is unlikely to accurately predict fatigue crack growth in cracked hollow section T joints. Bowness & Lee (1995b) evaluated this model and pointed out that the problems of this model may be caused by the drawbacks of the PRETUBE mesh, which is lack of a dense weld mesh to represent the highly nonlinear stress field caused by notch stress at the weld toe.

As a result of availability of more powerful software and rapidly falling computational costs, full 3D solid element models were widely used to analyse hollow section joints, such as Bowness & Lee (1995b), Qian(1996), Yang (1996) and Cao et al (1998). And it has been proved that the models using 3D solid elements for all tubes and welds can provide best results comparing to other two models above (Herion et al, 1996). However, The number of fatigue crack growth models based on full 3D solid element model is still few because of its complicated modelling. The key part using 3D solid element model is how to generate a good quality mesh for cracked hollow section joints, especially the mesh around the crack. The fatigue crack in the hollow section joint is a very complex geometry and has proved to be difficult to model (Bowness and Lee, 1995b) because this doubly curved crack not only curves around the intersection, but also curves under the weld toe (Bowness and Lee, 1998). This problem also exists

in Model (b). Normally, some commercial modelling programs, such as PRETUBE (1991), PATRAN (1993) and ANSYS (1996), can generate a mesh model for a hollow section joint automatically but it is still a time-consuming work using these programs to generate the mesh model, and the mesh quality cannot be guaranteed (Cao et al, 1998).

Yang (1996) used PATRAN and PRETUBE to produce a large number of models for uncracked and cracked hollow section joints and other cracked components. She found that it is not a simple matter to use PATRAN to generate meshes for hollow section joints, especially for those with cracks, and PRETUBE cannot generate good quality meshes for most cracked joints. So Bowness & Lee (1995b) and Cao et al (1997, 1998) have developed some special techniques that can change the complicated 3D mesh generation procedure to a two-dimensional (2D) procedure and make the modelling of welds and cracks simpler. These methods have been used to model different types of cracked hollow section joints in their studies (Bowness & Lee, 1995b, Cao et al, 1997). In this study, a new technique of automatically producing full 3D finite element models for cracked and uncracked SSHS T joints will be introduced. This technique was based on the work of Wong (2001) and Huang (2002) and the details about it will be given in Chapter 5.

2.6 ESTIMATION OF SIF IN HOLLOW SECTION JOINTS

The application of fracture mechanics principles bears largely upon the SIF. An essential part of a fracture problem in LEFM is the establishment of the SIF for the crack problem under consideration. Since the introduction of the fracture mechanics, much effort has been put into the derivation of SIFs. Up to today, SIF solutions are available for a wide range of geometrical configurations and many solutions have been collected (Tada et al, 1973). Unfortunately, in practical problems, for example SSHS joints, fatigue cracking of SSHS joints is an extremely complex problem since the surface crack path is usually contrasted to the weld toe and curves in the depth direction. This warped shape can often lead to a mixed mode component of crack

growth even for pure Mode I loading. At the same time, evaluation of the SIF for the actual problem using standard methods may be very expensive in both time and money. Up to now, the easier and cheaper way to calculate SIFs in cracked hollow section joints is the finite element method. In numerical approach, there are two commonly used methods for obtaining SIFs in cracked hollow section joints, namely displacement extrapolation and J-integral.

2.6.1 Displacement Extrapolation Method

Displacement extrapolation method was proposed by Aliabadi & Rooke (1991), and it has been used by many other researchers (Chong Rhee et al, 1991; Bowness & Lee, 1995) for cracked hollow section joints. This method uses the elastic solution of the displacements in the vicinity of the crack tip (Eqns. (2.6b), (2.7b) and (2.8b)). Bowness & Lee (1995) chose the largest displacements in the vicinity of the crack obtained from the paths up the crack face, i.e. θ equals $\pm 180^\circ$, to evaluate the mixed mode SIFs. By this method, the Eqns. (2.6b) to (2.8b) can be reduced to the following form, with all the other displacements being zero,

$$K_I = \frac{G}{2(1-\nu)} \sqrt{\frac{2\pi}{r}} v_n \quad (2.27)$$

$$K_{II} = \frac{G}{2(1-\nu)} \sqrt{\frac{2\pi}{r}} u_r \quad (2.28)$$

$$K_{III} = G \sqrt{\frac{2\pi}{r}} w_t \quad (2.29)$$

where u_r , v_n , w_t denote the local radial, normal and tangential displacements of the nodes on the crack surface (see Figure 2.7), G is the shear modulus and ν is the Poisson's ratio.

After the SIFs on a radial path near the crack tip are calculated, the crack front SIFs can then be found by extrapolating the linear part of the SIF distribution on the radial path back to the crack tip. This method is only valid in the regions very close to the crack tip, and it requires very fine crack tip meshes. The problem of this method is that the large variation occurs when calculating SIFs from different radial paths where the crack front meets the weld toe (Chong Rhee et al, 1991). In order to get the results in this region, Bowness & Lee (1995) just simply averaged the SIF values obtained from different radial paths.

2.6.2 J-integral Method

J-integral is a measure of strain energy in the region of crack tip. For a single crack mode, J-integral can be directly related to the SIF in the elastic regime as follows:

$$K = \sqrt{JE'} \quad (2.30)$$

where K is the SIFs, J is J-integral and $E' = E$ for plane stress and $E' = E/(1-\nu^2)$ for plane strain. E is the Young's modulus.

It has been found that J-integral has the advantage of being rather insensitive to mesh refinement, but can not easily be used in the mixed mode situations thought to be present in hollow section joints (Rhee and Salama, 1987 and Ritchie et al, 1986). Fu et al (1993) had analyzed a 3D T-butt model using J-integral, and had found another problem with J-integral that it lacked path independence in the region where the crack meets the weld toe. They attributed this to be a consequence of the presence of two stress singularities - one due to the weld toe and the other due to the crack tip - at this point.

To solve the mixed crack mode problem, Shih & Asaro (1988) had proposed another relationship between the J-integral and the SIFs. The expression is given as:

$$J = \frac{I}{8\pi} K^T \cdot B \cdot K \quad (2.31)$$

where $K = [K_I, K_{II}, K_{III}]^T$, and B is called the pre-logarithmic energy factor matrix. For homogenous, isotropic material, B is a diagonal matrix. In this case, Eqn. (2.31) can be simplified as

$$J = \frac{I}{\bar{E}} (K_I^2 + K_{II}^2) + \frac{I}{2G} K_{III}^2 \quad (2.32)$$

where $\bar{E} = E$ for plane stress, $\bar{E} = E/(1 - \nu^2)$ for plane strain, axisymmetric and 3-D problems, and G is the shear modulus.

It is obvious in Eqn. (2.32) that the J-integral can be easily worked out with the presence of K_I , K_{II} and K_{III} . However, K_I , K_{II} and K_{III} cannot be directly obtained from Eqn. (2.32). To solve this problem, Shih & Asaro (1988) had proposed an interaction integral method to calculate K from J-integral. In this method, the J-integral for a given problem can be written as:

$$J = \frac{I}{8\pi} \left[K_I B_{II}^{-1} K_I + 2K_I B_{I2}^{-1} K_{II} + 2K_I B_{I3}^{-1} K_{III} \right] + (\text{terms not involving } K_I) \quad (2.33)$$

Then Shih & Asaro (1988) had defined the J-integral for an auxiliary, pure Mode-I, crack-tip field with SIF K_I , as:

$$J_{aux}^I = \frac{I}{8\pi} k_I \cdot B_{II}^{-I} \cdot k_I \quad (2.34)$$

Superimposing the auxiliary field onto the actual field yields

$$J = \frac{I}{8\pi} \left[(K_I + k_I) B_{II}^{-I} (K_I + k_I) + 2(K_I + k_I) B_{I2}^{-I} K_{II} + \right. \\ \left. 2(K_I + k_I) B_{I3}^{-I} K_{III} + (\text{terms not involving } K_I) \right] \quad (2.35)$$

Since the terms not involving K_I or k_I in J_{tot}^I and J are equal, Shih & Asaro (1988) had defined the interaction integral J_{int} as:

$$J_{int}^I = J_{tot}^I - J - J_{aux}^I = \frac{k_I}{4\pi} (B_{II}^{-I} K_I + B_{I2}^{-I} K_{II} + B_{I3}^{-I} K_{III}) \quad (2.36)$$

If the calculations are repeated for Mode-II and Mode-III, a linear system of equations can be obtained as follow:

$$J_{int}^\alpha = \frac{k_\alpha}{4\pi} B_{\alpha\beta}^{-I} K_\beta, \quad (\alpha = I, II, III) \quad (2.37)$$

If the k_α is assigned with unit values, the Eqn. (2.37) can be reduced to a form as

$$K = 4\pi \cdot B \cdot J_{int} \quad (2.38)$$

where $J_{int} = [J_{int}^I, J_{int}^{II}, J_{int}^{III}]$. Then the problem reduces to calculating the J_{int} and the method used for calculating the J-integral can also be employed accordingly. The detailed derivation and the calculation of the J_{int} can be found in ABAQUS (2004), and Shih & Asaro (1988). This method has the advantage of being rather insensitive to

mesh refinement, and it can also be used for calculating the SIFs in the mixed mode problems.

2.7 CRACK SURFACE

If a static or fatigue loading is applied to a body containing a crack then major fracture mechanics objectives are to determine the conditions under which the crack will grow, together with the path taken by the crack. The complete solution of a crack growth problem therefore includes determination of the crack path (Pook, 2000). At the present state of the art the factors controlling the path taken by a propagating crack are not completely understood. In general crack paths are difficult to predict, and in practice crack paths in structures are often determined by large-scale structural tests (Salama et al, 1993).

In describing the paths taken by a growing crack it is necessary to describe a crack growth surface which contains successive positions of the crack front, and also a family of lines on this surface which describes successive positions of the crack front. The line which defines the crack surface is known as crack propagation direction. Based on the works by Ritchie et al (1986) and Erdogan and Sih (1963), three criteria are commonly used:

- the maximum tangential stress criterion;
- the maximum energy release rate criterion; or
- the $K_{II} = 0$ criterion.

K_{III} is not taken into account in any of these criteria. Bowness and Lee (1998) gave one way to calculate the crack propagation direction based on the $K_{II} = 0$ criterion. Figure 2.8 shows how these stress components are defined in the vicinity of the crack tip. $\tau_{r\theta}$ may be expressed in terms of the modes I and II SIFs:

$$\tau_{r\theta} = \frac{I}{2\sqrt{2\pi r}} \cos\left(\frac{\theta}{2}\right) [K_I \sin \theta + K_{II} (3 \cos \theta - 1)] \quad (2.39)$$

setting $\tau_{r\theta} = 0$ to satisfy the maximum tangential stress criterion, the following equation can be obtained,

$$K_I \sin \theta + K_{II} (3 \cos \theta - 1) = 0 \quad (2.40)$$

rearrange Eqn. (2.40), the crack extension angle can be derived:

$$\theta = 2 \tan^{-1} \left[\frac{1}{4} \frac{K_I}{K_{II}} \pm \frac{1}{4} \sqrt{\left(\frac{K_I}{K_{II}} \right)^2 + 8} \right] \quad (2.41)$$

The crack propagation angle θ is measured with respect to the crack plane and $\theta = 0$ represents the crack propagation in the “straight-ahead” direction.

In ABAQUS-6.4 (2004), crack propagation direction is given in form of $\hat{\theta}$. $\hat{\theta}$ is defined as:

$$\hat{\theta} = \cos^{-1} \left(\frac{3K_{II}^2 + \sqrt{K_I^4 + 8K_I^2 K_{II}^2}}{K_I^2 + 9K_{II}^2} \right) \quad (2.42)$$

where the crack propagation angle $\hat{\theta}$ is measured with respect to the crack plane and $\hat{\theta} = 0$ represents the crack propagation in the “straight-ahead” direction. $\hat{\theta} < 0$ if $K_{II} > 0$ while $\hat{\theta} > 0$ if $K_{II} < 0$. The crack propagation angle is measured from q to n;

that is, it is measured about the direction – t or counterclockwise measured from q as shown in Figure 2.9. The details are described in ABAQUS-6.4(2004).

In previous work on crack growth behaviour and evaluation of SIF solutions, crack surface has been largely ignored with a straight crack perpendicular to the chord surface being modeled. This is because of the extreme difficulty in modelling the actual situation of a crack surface curving under the weld toe as well as around the hollow section joint intersection. However, precise modelling is essential to the proper understanding of crack growth behaviour and assessment of the accuracy of SIF solutions.

Figure 2.1 Common rectangular tubular joint configuration

Figure 2.2 Parametric notation in square tubular T-joint

Figure 2.3 Position of lines A to E

Figure 2.4 Typical S-N curves for fatigue design of CHS and RHS joint (IIW, 1999 and Zhao et al, 2000).

Figure 2.5 Stress range for constant amplitude loading

Figure 2.6 Conventions for surface crack displacements

Figure 2.7 Coordinate system and stress components ahead of a crack tip

Figure 2.8 Three stages of fatigue crack growth

Figure 2.9 Polar coordination at crack tip

Table 2.1 Equations for the S_{rhs} - N_f curves for CHS joints ($4 \text{ mm} \leq t \leq 50 \text{ mm}$) and RHS joints ($4 \text{ mm} \leq t \leq 16 \text{ mm}$) (IIW, 1999 and Zhao et al, 2000).

for $10^3 < N_f < 5 \times 10^6$	$\log(S_{rhs}) = \frac{1}{3}(12.476 - \log(N_f)) + 0.06 \log(N_f) \log\left(\frac{16}{t}\right)$ <p>or</p> $\log(N_f) = \frac{12.476 - 3 \log(S_{rhs})}{1 - 0.18 \log\left(\frac{16}{t}\right)}$
for $5 \times 10^6 < N_f < 10^8$ (variable amplitude only)	$\log(S_{rhs}) = \frac{1}{5}(16.327 - \log(N_f)) + 0.402 \log\left(\frac{16}{t}\right)$ <p>or</p> $\log(N_f) = 16.327 - 5 \log(S_{rhs}) + 2.01 \log\left(\frac{16}{t}\right)$

CHAPTER 3

EXPERIMENTAL TESTING OF SQUARE-TO-SQUARE HOLLOW SECTION T-JOINTS (PART I-STATIC TEST)

3.1 INTRODUCTION

The fatigue behavior of the joints under cyclic loading is one of the major concerns in the design of hollow section joints structures. Towards this end, many studies have been carried out to determine the fatigue performance of the welded joints (Frater and Packer 1993, Puthli et al, 1986 and 1988, Strommen and Larsen 1994, van Wingerde 1992) and the results have been included in CIDECT (Zhao et al, 2000) and many other design guidelines. In general, the related studies belong to two main categories, namely the S-N approach and the fracture mechanics method. The S-N approach is used to predict the total life of a joint under service load but cannot be used to assess the residual life of a joint which has a crack-like default. As compared with the first approach, the later one has been proved to be a reliable approach when assessing the residual life of a joint with an existing default or crack. That means that the fracture mechanics method is to deal with crack growth not crack initiation. However, the application of fracture mechanics method largely depends on crack information, such as crack path, crack surface, and crack shape etc which can only be obtained from practice or fatigue testing. Hence, benchmark studies that can provide such information becomes very necessary and important. In this sense, large-scale fatigue tests that focus on the fatigue performance should be carried out.

Up to today, large-scale fatigue tests on hollow section joints have been performed for a number of decades. On the other hand, the fatigue performance including the crack

propagation and the residual life of a hollow section joint were not studied during that time because most of those tests aimed to study the stress concentration factor (SCF) and the fatigue life of certain dimension joints, to finally generate a S-N curve (ECSC research project and CIDECT research project). Until recently, fatigue performance study based on large-scale fatigue tests have been carried out (Dijkstra and Noordhoek, 1985, Huang, 2003 and Shao, 2004). However, all these tests have been conducted for circular hollow section (CHS) joints.

For rectangular hollow sections (RHS), although there exist some equations about SCF under axial load (AX) and in-plane bending (IPB) (Zhao et al, 2000), the SCF equations under out-of-plane bending (OPB) are not available yet and this limits utilisation of the superposition method, which is used to calculate the hot spot stress (HSS) under combined loads including OPB. In order to calculate the fatigue life of a RHS joint, S-N curves have been predominately used in design. However, few publications have been found about the calculation of residual life of RHS joints with existing defect or initial crack, though initial defect or crack size is assumed to be 0.8mm in many codes. The crack propagating information, such as crack location, crack surface and crack path has not been reported. Actually, the understanding about the crack propagation in RHS joints is still limited.

Arising from above discussions, tests on large-scale square-to-square hollow section (SSHS) T-joints are conducted in this study. The objectives of these tests are to study the fatigue performance of the SSHS T-joints, including the SCF distribution under static loading and the crack propagation under cyclic loading. These tests present the actual states of the fatigue performance and provide rare information for later numerical modelling. Four full-scale SSHS T-joints subjected to cycle loading are tested until fatigue failure.

Procedure and findings of the test study will be reported in two parts. Experimental rig and static tests will be introduced in Part I (this chapter). Fatigue test and the fatigue

test results will be described in Part II (Chapter 4). In static tests, SCF distribution under three basic loads is obtained and the SCF value is used to verify the existing equations (Zhao et al, 2000). The SCF value is also used to verify the superposition method so as to get the hot spot stress distribution under various load conditions using superposition method. In fatigue tests, crack initiation and development are monitored by ACPD technique. Crack shape and crack surface under weld toe are measured. The fatigue life corresponding to certain hot spot stress range of each specimen is used to validate the UK DEn S-N curve (Zhao et al, 2000).

3.2 TEST SETUP AND SPECIMEN DETAILS

In this study, the “orange” rig designed specifically for uniplanar hollow section joints under AX, IPB, OPB or any combinations of these three basic load cases shown in Figure 3.1 is used. The rig is capable of applying static loads to determine the SCF and HSS distributions around the joint as well as cyclic loads to study its fatigue life and fracture behaviours. As shown in Figure 3.2, three actuators, namely, actuator 1, actuator 2 and actuator 3 are used to apply loads along three mutually perpendicular axes. Actuator 1 and actuator 2 with capacity of 250KN are employed to apply AX and IPB loads to the specimen respectively, while actuator 3 with capacity of 150KN is employed for the generation of OPB load. The actuators can be operated individually or concurrently to create combined load conditions.

The four T-joint specimens were fabricated from square hollow sections. The dimensions, orientation and non-dimensional parameters of the joints are given in Figure 3.3. Structural steel pipes to EN10025 - S355 J2H are used for the specimens. The pipes are hot finished. The mechanical properties of the steel are given in Table 3.1. The weld profiles and the specimen preparation are carried out in accordance with the AWS Structure Welding Code D1.1-2000 (2000) specification. They are then checked using the ultrasonic technique and the results confirm that the quality of the welds is acceptable, and in compliance with the standard specifications. Three 22.0 mm thick plates are welded to the two ends of the chord and the brace end of the specimens

respectively to facilitate the specimen installation and load application as shown in Figure 3.1.

The weld size has a significant effect on the stress distribution around a hollow section joint. Therefore, it is crucial to know the actual weld profile in practice. Figure 3.4 shows the actual weld sizes of the four T-joints specimens measured using weld gauge and the minimum value of the weld size defined in AWS (2000).

3.3 STRAIN GAUGE LOCATION

In order to capture the strain distribution around the brace-to-chord connection, 40 points on chord (CG1 to CG40) and 32 points on brace (BG1 to BG32) are firstly selected to measure their strains in Specimen I. The five specific points (A, B, C, D and E) at the four corners (CP1 to CP4) defined by Puthli et al (1986), which have equations to calculate SCFs under AX and IPB load in CIDECT guideline (Zhao et al, 2000), are included in these points as shown in Figure 3.5(b). Arising from the results of Specimen I, the job of putting strain gauges can be simplified. Hence, in Specimen II, III and IV, 24 points on chord (CG1 to CG24) and 20 points (BG1 to BG20) on brace are selected to measure their strains including the five specific points (A-E) at each corner (CP1 to CP4), which is shown in Figure 3.5(c). van Wingerde (1992) pointed out that for RHS joints, the quadratic extrapolation is required because of the strong non-linear strain distribution near the weld toe. The quadratic extrapolation definition is shown in Figure 3.6 and the quadratic extrapolation zone is tabulated in Table 3.2. Hence, three strain gauges are arrayed along the line perpendicular to the weld toe with the leading end at each interesting point (CG1 to CG40 and BG1 to BG32 in Specimen I, and CG1 to CG24 and BG1 to BG20 in Specimens II, III and IV). The distance to weld toe is $0.4t$, $0.9t$ and $1.4t$ respectively (t is the thickness of tube members), which is in the extrapolation zone. These perpendicular gauges are glued onto the specimens in such a way that they measure the distribution of perpendicular strain component ($\epsilon_{\perp}$) around the intersection. The strain gauge distribution around the connection is shown in

Figure 3.5 (b) and (c). At the four typical corners, i.e. CP1, CP2, CP3, CP4, as shown in Fig. 3.5, another four sets of parallel strain gauges are glued onto the specimen at point C to measure the strain components parallel (ε_{II}) to the weld toe (please refer to the enlarged view in Figure 3.5(b) and (c)). At all locations, the value $\varepsilon_{\perp}$ and ε_{II} at the weld toe are obtained by applying quadratic extrapolation to the three strain gauge readings logged during the static test. Finally, four additional strain gauges are placed at the center of the brace side respectively to monitor the actual brace load applied. The full view of strain gauge location is shown in Figure 3.5 (a). In Specimen I, 3mm length stain gauges are put and in Specimen II, III and IV, 1mm length strain gauges are adopted due to the limit of the extrapolation zone. One typical strain gauge putting is shown in Figure 3.7. During the test, the strain gauges were connected to three ASW-50 switch-boxes and one TDS-801 data logger. The “Labwindows” software on a PC was then used to monitor the real-time data collection.

3.4 APPLICATION OF STATIC TEST LOAD

In the static test, a series of load cases consisting of AX, IPB, OPB, and arbitrary combinations of AX, IPB and OPB are chosen and tested. The basic load cases of AX, IPB and OPB are employed to obtain the SCFs of the specimens, and to investigate the validity of the published SCF equations (Zhao et al, 2000). The load cases of arbitrary combinations of AX, IPB and OPB are used to evaluate the superposition method suggested by Gulati et al (1982) to calculate the stress distribution under combined loading. The stress, σ_p , at a given point P at the weld toe under combined loadings is calculated as

$$\sigma(P) = SCF_{AX}(P) \cdot \sigma_{n-AX} + SCF_{IPB}(P) \cdot \sigma_{n-IPB} + SCF_{OPB}(P) \cdot \sigma_{n-OPB} \quad (3.1)$$

where $SCF_{AX}(P)$, $SCF_{IPB}(P)$ and $SCF_{OPB}(P)$ are the stress concentration factors at point P at the intersection of the joint subjected to AX, IPB and OPB respectively, and σ_{n-AX} , σ_{n-IPB} and σ_{n-OPB} are the corresponding nominal stresses.

Prior to the measurement of strains, each specimen is subjected to six loading and unloading sequences to ensure that the strain readings are stabilized. This loading procedure ensures a satisfactory performance of strain gauges and eliminates the drift or any hysteresis of strain measurements due to the fabrication of the joints. For the actual tests, the loads applied to each actuator are pre-programmed before the test. 70% of the material yield stress σ_y (Table 3.1) is used to calculate the upper-limit value of the load applied during static tests. It is ensured that the joint behaves elastically under the maximum pre-programmed load. During the static load tests, the actuators are ramped to the maximum predetermined loads in three steps. The actuators are controlled by the means of RS Console software that is developed especially for the servohydraulic system. At each load step, the actuators are held in place under active feedback load control while the strain readings are recorded. The loads are then increased to the next load step, and held in place while the strain readings are again taken. After reaching the maximum load, the above steps are repeated by releasing the applied load in three steps to zero.

3.5 STATIC TEST RESULTS

After the test, strain gauge readings are analyzed and the hot spot strain (HSSN) is obtained using quadratic extrapolation method. The quadratic extrapolation equation is as follows according to the distance of the strain gauges to the weld toe,

$$HSSN = 2.52 \cdot Sn(1) - 2.24 \cdot Sn(2) + 0.72 \cdot Sn(3) \quad (3.2)$$

where $Sn(1)$, $Sn(2)$, and $Sn(3)$ is strain gauge readings along the line perpendicular to the weld toe, with the distance to weld toe $0.4t$, $0.9t$ and $1.4t$ respectively.

Then the strain concentration factor (SNCF) is obtained according to the following equation,

$$SNCF = \frac{HSSN}{SN_{nominal}} \quad (3.3)$$

where $SN_{nominal}$ is the nominal strain computed from the readings of strain gauges at the center of brace sides.

After SNCF is obtained, the SCF can be calculated. There exists the relationship between SNCF and SCF as follows,

According to Hooke's Law for plane stress, there exists,

$$\sigma_{\perp} = \frac{E}{1-\nu^2} (\varepsilon_{\perp} + \nu \varepsilon_{\parallel}) \quad (3.4)$$

where $\sigma_{\perp}$ is the stress component that is perpendicular to the weld toe; E is Young's modular; ν is Poisson ratio and $\varepsilon_{\perp}$, $\varepsilon_{\parallel}$ is the stain component perpendicular to the weld toe and parallel to the weld toe respectively. Since the stress that perpendicular to the weld toe is adopted in the defining of the SCF, there have,

$$SCF = \frac{\sigma_{\perp}}{\sigma_n} = \frac{E}{1-\nu^2} \left(\frac{\varepsilon_{\perp}}{\sigma_n} + \nu \frac{\varepsilon_{\parallel}}{\sigma_n} \right) \quad (3.5)$$

where σ_n is nominal stress, which can be expressed as, $\sigma_n = E\varepsilon_n$ (ε_n is nominal strain). Hence,

$$\begin{aligned} SCF &= \frac{I}{1-\nu^2} \left(\frac{\varepsilon_{\perp}}{\varepsilon_n} + \nu \frac{\varepsilon_{\parallel}}{\varepsilon_n} \right) = \frac{I}{1-\nu^2} \left(SNCF + \nu \frac{\varepsilon_{\parallel}}{\varepsilon_{\perp}} \frac{\varepsilon_{\perp}}{\varepsilon_n} \right) \\ &= \frac{I}{1-\nu^2} \left(SNCF + \nu \frac{\varepsilon_{\parallel}}{\varepsilon_{\perp}} SNCF \right) = \frac{I}{1-\nu^2} \left(1 + \nu \frac{\varepsilon_{\parallel}}{\varepsilon_{\perp}} \right) SNCF \\ &= Snf \cdot SNCF \end{aligned} \quad (3.6)$$

where Snf is called stress-strain conversion factor. Thus, the Snf can be expressed as:

$$Snf = \frac{I}{1-\nu^2} \left(1 + \nu \frac{\varepsilon_{\parallel}}{\varepsilon_{\perp}} \right) \quad (3.7)$$

From Eqn. (3.7), for the “ideal” situation that $\varepsilon_{\perp}$ is the only non-zero strain component and $\varepsilon_{\parallel} = 0$, one has $Snf = 1/0.91 \approx 1.10$ which is the value proposed by Dutta (1996). This value of 1.10 has widely been used in RHS joints. In order to check the validity of this value at the corners of the joint, experimental mean values based on parallel strain gauge readings at the corners (please refer to Figure 3.5) under basic loading cases are computed and listed in Table 3.3. From the table, it can be seen that all the experimental $Snfs$ are higher than 1.1, equivalent to a range of 16% ($Snf = 1.152$) to 34% ($Snf = 1.211$) for the ratio $\varepsilon_{\parallel} / \varepsilon_{\perp}$. This implies that due to the presence of sharp corners, $\varepsilon_{\parallel} \neq 0$ and adopting $Snf = 1.1$ will result in an underestimation of the SCF. Hence, for RHS joints a value of $Snf = 1.2$ appears to be more appropriate.

3.5.1 SCF Computation

After Snf is determined, for basic load cases, the strain obtained from the test can be directly used for the deduction of SCF values. The experimental SCF values measured at the five selected points (Figure 3.5) on chord (D, C and B) and brace (E and A) at the

corners under different basic loads are tabulated in Table 3.4. The experimental SCF values listed in Table 3.4 are the averages at the corners and they were computed from the SNCFs by using the corresponding S_{nf} values listed in Table 3.3. The SCFs at these points obtained by using the equations given by van Wingerde (1992) and adopted in the CIDECT Guide No.8 (Zhao et al, 2000) are also listed in Table 3.4. It can be seen that the two sets of SCFs roughly agree at point B, but not at other points. Note that in the CIDECT guide, one equation is provided for both points A and E and no equation is given for the case of OPB. A closer experimentation of the results listed in Table 3.4 indicates that:

- (1) In all the load cases, SCFs at chord are greater than that at brace.
- (2) At point E, values predicted by the equations are too high and different equations should be used at points A and E.
- (2) For both AX and IPB cases, the peak SCFs values appeared at points B or C. For the OPB case, the maximum SCFs values appeared at point B.
- (3) More importantly, for AX and IPB load cases, the SCFs at point B are higher than that at point D (except for Specimen I under AX load where they are practically equal).

From the above observation, one can predict that under cyclic loading, *even without any OPB loading*, surface crack will be initiated at region between point B and D on the chord surface and propagate mainly in the direction parallel to the chord wall (e.g. along the lines CP1-CP2 or CP3-CP4).

3.5.2 SCF Distributions around the Brace-Chord Intersection

The SCF distributions under basic load are then analyzed. The SCF values are the mean values of all the AX, IPB and OPB basic load conditions respectively. The SCF distributions in Specimen I are shown in Figures 3.9 and 3.10, since more interesting points are investigated (40 points on chord and 32 points on brace). It can be seen that

the peak SCF is always located at the corner (CP1 to CP4) on chord under AX and IPB. Under OPB, the peak SCF sometimes is located at CG9 and CG29 which is not in the range of the corner and SCF values at the points along the chord wall are all very large (please refer to Figure 3.5). The peak SCF values on brace are not located at either point A or point E. The point at the centre of the corner arc has the largest SCF values. Based on the results of Specimen I, the strain gauge application can be reduced. At some points that are not critical, the strain gauge application can be exempted. Hence, the interesting points are reduced to 24 points on chord and 20 points on brace (as shown in Figure 3.5). SCF distributions around the connection in Specimen II, III and IV are shown in Figures 3.11 to 3.16. The same conclusions can be drawn from these figures.

The three Specimens II, III and IV have same parameters β , and γ . The τ value is reduced from 1.0 to 0.75 to 0.656. According to the conclusions in CIDECT (Zhao et al, 2000), large amount SCFs will appear on chord with larger τ value; and the effect of τ on SCFs on brace is neglected. However, from Figures 3.11 to 3.16, this rule is not visualized clearly. For example, for most of the case, the chord SCFs have the smallest values in Specimen III which has the τ value of 0.75. The brace SCFs exhibit the inverse trend. The brace has the smaller SCF values while larger SCF values appear on chord. This difference from the CIDECT Design Guide No.8 conclusion may be caused by the weld size. Another interesting point is that the effect of β on SCF under AX is larger than that under IPB and OPB.

From Figures 3.9 to 3.16, it can be seen that the peak SCF is located on chord surface under all load conditions for these four specimens. It means that crack should initiate and propagate in chord when fatigue loads are applied to these four specimens. It can be seen from Table 3.4 that the hot spot locations predicted by CIDECT Design Guide No.8 matches the experimental ones although the predicted SCFs are slightly larger than those obtained in the tests.

3.5.3 Validation of Superposition Method

Comparisons are also made between the stress distribution obtained by experimental results under combined loads and those obtained by superposition of basic loading cases to validate the superposition method. Eqn. (3.1) is used to calculate stresses based on the principle of superposition. The SCF-factors in Eqn. (3.1) are obtained from the SCF results of tests for the basic load cases at different locations of the brace and chord intersection. Detailed comparisons show that in all the combined load cases and specimen tested, the stress distributions obtained by the superposition method agree well with the experimental measurements. Three of the comparisons from Specimen III are shown in Figure 3.17 to Figure 3.22. The nominal stresses of the load cases in the three comparisons are as follows,

Load case 1, $\sigma_n(AX) = 2.94MPa$ and $\sigma_n(IPB) = 7.64MPa$;

Load case 2, $\sigma_n(AX) = 3.02MPa$ and $\sigma_n(OPB) = 6.72MPa$;

Load case 3, $\sigma_n(IPB) = 10.50MPa$ and $\sigma_n(OPB) = 9.45MPa$

Noted that all nominal stresses are calculated from the four midway brace member strain gauge readings directly. Hence, it can be concluded that the superposition method can be applied to obtain the HSS for RHS joints under combined load cases.

Figure 3.1 Test rig and specimen installation

Figure 3.2 Actuators to apply loads

Figure 3.3 Specimen details

Figure 3.4 Weld details of the four specimens

(a) Full view of strain gauge location

(b) Plane view of strain gauge location in Specimen I

(c) Plane view of strain gauge location in Specimen II, III and IV

Figure 3.5 Strain gauge location on the specimens

Figure 3.6 Extrapolation zone

Figure 3.7 A close-up view of strain gauge putting around the intersection

Figure 3.8 Close-up view of parallel strain gauge at corner CP1

Figure 3.9 SCF distribution on chord of Specimen I under AX, IPB and OPB

Figure 3.10 SCF distribution on brace of Specimen I under AX, IPB and OPB

Figure 3.11 SCF distribution on chord of Specimen II, III and IV under AX

Figure 3.12 SCF distribution on brace of Specimen II, III and IV under AX

Figure 3.13 SCF distribution on chord of Specimen II, III and IV under IPB

Figure 3.14 SCF distribution on brace of Specimen II, III and IV under IPB

Figure 3.15 SCF distribution on chord of Specimen II, III and IV under OPB

Figure 3.16 SCF distribution on brace of Specimen II, III and IV under OPB

Figure 3.17 HSS comparison on chord between experimental and superposition results under AX25KN+IPB(-2)KN in Specimen III

Figure 3.18 HSS comparison on brace between experimental and superposition results under AX25KN+IPB(-2)KN in Specimen III

Figure 3.19 HSS comparison on chord between experimental and superposition results under AX25KN+OPB(-2)KN in Specimen III

Figure 3.20 HSS comparison on brace between experimental and superposition results under AX25KN+OPB(-2)KN in Specimen III

Figure 3.21 HSS comparison on chord between experimental and superposition results under IPB(-2)KN+OPB(-2)KN in Specimen III

Figure 3.22 HSS comparison on brace between experimental and superposition results under IPB(-2)KN+OPB(-2)KN in Specimen III

Table 3.1 Mechanical properties of the four specimens

Specimen	Yield strength (MPa) 1	Ultimate tensile strength (MPa) 2	Elongation (%) 3
I	366	511	32
II	370	518	32
III	370	518	32
IV	370	518	32

Table 3.2 Boundaries of extrapolation region for RHS joints

Distance from weld toe		chord	brace
RHS	$L_{r,max}$	$0.4t_0$	$0.4t_1$
	$L_{r,min}$	$L_{r,min}+t_0$	$L_{r,min}+t_1$
Note: Minimum value for $L_{r,min}$ is 4mm			

Table 3.3 Snf calculated from experimental results

Snf	AX	IPB	OPB
Specimen I	1.152	1.173	1.186
Specimen II	1.193	1.158	1.172
Specimen III	1.162	1.170	1.193
Specimen IV	1.178	1.181	1.211

Table 3.4 Experimental SCF values obtained from static test

Specimen		AX			IPB			OPB
/Location		Exp.	Eqn.	Diff.(%)	Exp.	Eqn.	Diff.(%)	Exp.
I	D	15.26	6.65	-56.4	5.32	7.05	32.52	2.29
	C	17.52	14.53	-17.11	10.45	13.2	26.3	6.38
	B	15.25	16.19	6.09	9.93	10.68	7.55	10.18
	A	12.48	9.94	-20.35	8.16	7.3	-10.54	6.59
	E	6.26	9.94	50.83	5.88	7.3	24.15	2.33
II	D	12.06	9.70	-19.50	5.94	8.35	40.57	2.26
	C	21.74	20.11	-7.50	12.56	14.45	15.05	8.51
	B	21.84	22.43	2.70	9.86	10.51	6.59	13.52
	A	9.39	12.35	31.50	4.44	6.25	40.76	4.93
	E	2.00	12.35	517.50	2.75	6.25	127.17	0.84
III	D	10.52	8.05	23.48	4.69	6.93	47.76	1.87
	C	15.40	16.69	8.38	8.25	11.99	45.33	6.62
	B	13.06	18.62	42.57	5.24	8.72	66.41	7.96
	A	10.48	12.35	17.80	4.43	6.25	41.08	5.45
	E	2.94	12.35	320.06	2.89	6.25	116.26	1.16
IV	D	11.64	7.6	-34.71	6.57	6.52	-0.76	2.45
	C	13.31	15.96	19.91	8.74	11.66	33.41	5.95
	B	14.06	17.93	27.52	8.98	8.49	-5.46	10.37
	A	11.85	13.34	12.57	6.65	6.76	1.65	6.10
	E	5.36	13.34	148.88	5.09	6.76	32.81	2.07

Note: **Exp.** = Experimental SCF values

Eqn. = SCF values predicted by using equations given in [van Wingerde 1992]

Diff. (%) = Percentage difference calculated as $100 \times (\text{Eqn.} - \text{Exp.}) / \text{Exp.}$

Maximum SCF values for each case are enclosed in boxes.

CHAPTER 4

EXPERIMENTAL TESTING OF SQUARE-TO-SQUARE HOLLOW SECTION T-JOINTS (PART II-FATIGUE TEST)

4.1 INTRODUCTION

Studies on the calculation of residual life for rectangular hollow sections (RHS) joints with an existing defect or an initial crack have been found in few publications. In order to investigate the fatigue crack initiation and propagation in thin SSHS joints, Zhao et al (2001) have carried out some fatigue testing about thin-wall SSHS T-joints. In their research work, different failure criteria were discussed. Investigations on the crack initiation, crack propagation, through thickness crack for SHS-SHS, CHS-CHS and CHS-SHS thin-walled T-joints ($t < 4\text{mm}$) were also conducted by Mahiri et al (2002, 2004a, 2004b) and Tong et al (2006). However, up to today, the crack propagating information in large-scale RHS joints, such as crack surface and crack path was not reported. Actually there is a lack of understanding about the crack propagation in large scale RHS joints.

In this study, large-scale square-to-square hollow section (SSHS) T-joint tests are carried out to study the fatigue performance of the SSHS T-joints, including the distribution of stress concentration factor (SCF) under static loading and the crack propagation under cyclic loading. These tests will present the actual states of the fatigue performance and provide rare information for later numerical modelling.

The static tests and experimental rig introduced in Chapter 3 have provided enough stress information for fatigue tests. In this chapter, cyclic loads are applied to the four

specimens. The fatigue test procedure is described in detail and the test results are presented. During the tests, crack initiation and development are monitored by alternative current potential drop (ACPD) technique. Crack shape and crack surface under weld toe are studied using clay model. The fatigue life corresponding to certain hot spot stress range of each specimen is used to validate the UK DEn S-N curve (Zhao et al, 2000).

4.2 CYCLIC LOADS APPLIED AND STRESS DISTRIBUTION

In the fatigue tests, sinusoidal constant-amplitude loads, which are combinations of three basic loads with different components, are applied to the specimens respectively. The cyclic loading patterns applied by the Instron RS Console software to the four specimens are shown in Figures 4.1 to 4.4. These cyclic loads are selected in such a way that the resulted HSS at the weld toe are in the range of $[0.75\sigma_y, 0.85\sigma_y]$ so that the specimens would fail in a realistic numbers of load cycles (50k-250k). For all load cases, the minimum magnitude of the applied load is zero while the maximum loads generated a tensile stress at one of the four corners on the chord. From the static test results, it is known that the SCF under IPB and OPB is very large and the brace is very long. Hence, the IPB and OPB load applied at the end of the brace is very small to maintain the specimens in the elastic state. Hence, during fatigue testing, much attention must be paid to the IPB and OPB load. The actual nominal tensile stresses caused by peak cyclic loading applied to the specimens are calculated by the four midway brace member strain gauges readings concurrently and tabulated in Table 4.1. The distribution of hot spot stress calculated from the superposition method (Eqn. (3.1)) which has been validated in Chapter 3, around the connections of the specimens on chord, is shown in Figure 4.5, since the peak hot spot stress is located on chord for all the four specimens as proved in Chapter 3. During the tests, all the actuators are preset under load control condition.

4.3 ACPD TECHNIQUE

ACPD is a well-established technique (Dover et al, 1995) which can be used to monitor the process of crack initiation and propagation. ACPD uses an alternating current with a high frequency of about 5 kHz, which is induced on the surface of ferromagnetic material. When the current passes through a conductor, the so-called “skin-effect” δ forces the current to flow in a thin layer on the outer surface. Materials of high permeability and conductivity have small skin depth. For example, at a frequency of 5 kHz, ferromagnetic mild steel has a skin depth of the order of 0.1 mm.

Assuming there is a surface defect or crack as shown in Figure 4.6, the alternating current will flow down one surface crack and up the other crack side. A linear potential gradient is assumed to exist in the metal surface and the crack sides. Measurements of potential drops across the crack and adjacent to the crack allow the calculation of one dimensional crack depth as follows:

$$V_R \propto \Delta_R \quad (4.1)$$

$$V_C \propto (\Delta_C + 2d_1) \quad (4.2)$$

where V_R is the reference potential drop, V_C is the cross-crack potential drop, Δ_R is the reference probe gap, Δ_C is the cross-crack probe gap, and d_1 is the crack depth at that particular probe or site.

From Eqns. (4.1) and (4.2), the crack depth can be obtained as follow:

$$d_1 = (\Delta_R / 2) (V_C / V_R - \Delta_C / \Delta_R) \quad (4.3)$$

If Δ_C and Δ_R are same, Eqn. (4.3) reduces to

$$d_1 = (\Delta/2)(V_C/V_R - 1.0) \quad (4.4)$$

where Δ is the probe spacing.

Eqn. (4.4) is only applicable for an infinite long crack in an infinite plate. For a semi-elliptical crack in tubular joint, a modification factor should be used to modify the one-dimensional crack depth. Assuming the modification factor is M_x , then the true crack depth, d_2 , in tubular joints can be expressed as follow:

$$d_2 = M_x d_1 \quad (4.5)$$

The modification factor is usually a complicated function which is difficult to determine by a simple formula. However, fatigue cracks in tubular joints have been shown to maintain a low aspect ratio, often with $d_1/2c \approx 0.1$. It has been proved that this modification factor is less than 1.05 for cracks of this shape (Kam, 1985). Therefore, Eqn. (4.4) can be used for tubular joints without using this modification.

Eqn. (4.4) is derived using the assumption that the cross crack probe gap (Δ_C) and the reference probe gap (Δ_R) are same. However, inaccuracies in sitting the welded fixed probes mean that it can no longer be assumed that the probe gaps are equal, and Eqn. (4.3) should be used in this situation. But accurate determination of Δ_R and Δ_C is not easy, especially Δ_C because it is sited across the weld toe. Examination of the Eqn. (4.3) shows that the increment of crack growth between successive ACPD scans, d_1^1 and d_1^2 , is indeed independent of the cross crack probe spacing, Δ_C , as shown below:

$$d_1^2 - d_1^1 = \frac{\Delta_R}{2} \left(\frac{V_{C2}}{V_{R2}} - \frac{\Delta_C}{\Delta_R} \right) - \frac{\Delta_R}{2} \left(\frac{V_{C1}}{V_{R1}} - \frac{\Delta_C}{\Delta_R} \right) \quad (4.6)$$

Thus, only the reference probe gap spacing, Δ_R , is necessary to determine Δ_d .

A procedure proposed by Austin (1994) was used to calculate the actual reference probe spacing, Δ_R , from the raw reference probe data in this study. The first ten sets of reference probe readings, V_R , taken on the uncracked joint were averaged for each site. Assuming the nominal spacing is 10 mm, Δ_R can be back-calculated from the actual reference, V_R , as shown below:

$$\begin{aligned} V_R &\propto \Delta_R \\ V_R &= k\Delta_R \\ V_{10} &= k \cdot 10 \end{aligned} \tag{4.7}$$

Thus,

$$\Delta_R = 10(V_R/V_{10}) \tag{4.8}$$

Inaccuracies in sitting the welded fixed probes will also cause non-zero crack depth readings at some sites even before fatigue testing has commenced. Subtraction of these initial ACPD crack depth readings on the uncracked joint from all subsequent ACPD scans can give an accurate measure of crack development.

Generally, the calculation of the crack lengths from ACPD crack depth data is difficult because the probes are fixed and the probes may be 10 mm or more apart in the region where the crack ends would be expected to be in the later stages of life. Some attempts had been done before by Kam (1985). A simpler method is used in this study. A second order polynomial curve is used to fit the depths of the three probe sites nearest the crack ends. Then the length of the crack is determined by extrapolating this curve to the chord surface. The comparison between the crack lengths produced using this method

with that by optical measurements shows that it is suitable to estimate the crack lengths.

4.4 ACPD PROBES LOCATIONS

During fatigue test, ACPD technique is used to record crack development on the chord surface. Hence, ACPD probes, which are used to capture crack depth data, must be put around expected crack area. A crack is expected to initiate at peak the hot spot stress site and develop within this region. Therefore, based on the distribution of hot spot stress on each specimen as shown in Figure 4.5, ACPD probes are concentrated at region around corner CP3 for Specimen I and IV, and CP4 for Specimen II and III. The probe locations in Specimens I and II are arranged as Figures 4.7 and 4.8. From the testing results of Specimen I and II, it seems that the crack will not proceed to cross the middle line of the brace in the direction CP3-CP2 or CP4-CP1 and the crack length can be very long in the direction CP3-CP4 or CP4-CP3. Hence, the probes in Specimens III and IV are rearranged as shown in Figure 4.9 and 4.10. On each specimen, four fields are used and each field includes eight channels. Based on the dimensions of Specimen I, the distance between two adjacent channels is either 10mm or 20mm and a total length of 420mm is covered. For Specimens II to IV, probe interval of 10mm is adopted and the covered length is 310mm. In all these test studies, specially manufactured probes with diameter of 0.18 mm and length of 10 mm are used. A close-up view of these ACPD probes is shown in Figure 4.11. All the probes are connected to the U10 Crack Microgauge (TSC, 1991) using cables (Figure 4.12). This equipment with the capacity of utilizing the ACPD technique can detect the size of long shallow cracks from a minimum depth of about 0.1mm in electrically conducting material (Dover and Monahan, 1994). The U10 Crack Microgauge (TSC, 1991) is further connected to a computer which is controlled by running the Flair software (TSC, 1998). The Flair software (TSC, 1998) is written specifically for use of U10 Crack Microgauge (TSC, 1991) so as to provide automated instrument control, data storage facilities and dedicated graphical output under a windows environment.

4.5 GEOMETRICAL PARAMETERS

In order to facilitate the description of the fatigue testing results, definitions of some parameters used in the description of the geometry of a surface crack are given. As shown in Figure 4.13a, a surface crack is initiated at the peak HSS point $\mathbf{p}$ and *propagates* along the weld toe while it *penetrates* into the chord face. Assume that after a number of load cycles, the two endpoints $\mathbf{p}_1$ and $\mathbf{p}_2$ mark the extent of the crack. The *crack length*, l is then defined as the length of the curve $\mathbf{p}_1\mathbf{p}_2$. To describe the level of penetration into the chord face, a typical cross-section $I'-I'$ which cuts across the crack at point $\mathbf{O}'$ in such a way that it is perpendicular to the weld toe at $\mathbf{O}'$ is examined. A local coordinate system $x'-z'$ is set up with origin at $\mathbf{O}'$ and the deepest point at this section is denoted as $\mathbf{d}'$ (Figure 4.13b). The *curved crack depth*, a' is defined as the length of the curve $\mathbf{O}'\mathbf{d}'$. In addition, the *projected cracked depth*, a , is defined as the projected length of the curve $\mathbf{O}'\mathbf{d}'$ on the z' axis. To describe the geometry of the crack surface, a typical point $\mathbf{q}'$ on the curve $\mathbf{O}'\mathbf{d}'$ is considered. The angle $\theta_s(z')$ is defined as the angle between the line $\mathbf{O}'\mathbf{q}'$ and the z' axis. Note that if the variation of θ_s with respect to z' is known, an explicit mathematical expression of the curve $\mathbf{O}'\mathbf{q}'$ can be constructed which will be discussed in Chapter 5. In general, since the crack profile is a curve, $a' \geq a$ and it is possible that $a'/t_0 > 1$ while $a/t_0 \leq 1$. The shape of the curve $\mathbf{O}'\mathbf{d}'$ also varies along the crack length so that its shape at point $\mathbf{p}$ (Figure 4.13a) is different from that at section $I'-I'$. Furthermore, it is possible that maximum values of a' and a appear at different sections. The crack may penetrate through the chord (i.e. $a=t_0$) at one section while at another section, a' may attend a maximum value but still with $a/t_0 < 1$. Note that for CHS joints (Bowness and Lee 1995), it is assumed that $\mathbf{O}'\mathbf{d}'$ is a straight line so that θ_s is constant and $a'=a$.

4.6 FATIGUE TEST RESULTS

4.6.1 Introduction

During the fatigue test, two distinct phases can be identified [UK Den 1993]: The *initiation phase* is defined as the period between the first load cycle and N_2 , the cycle when the crack can be detected visually (or by the ACPD technique, i.e. $a' \geq 0.1\text{mm}$). The *propagation phase* is defined as the period between N_2 and N_3 , the cycles to through-thickness crack (TTC). The propagation (along the weld toe) and penetration (into the chord face) of the surface crack were monitored at the 32 ACPD probes locations such that the *curved crack depth*, a' , attended at different load cycles were scanned and recorded.

After the tests, a Fortran program is developed to analyze the readings of the raw crack depth and subtract the initial depth of the uncracked joint from all scans of the ACPD records. The plot of ACPD crack shape development for each specimen is presented. The crack profiles, i.e. crack length, crack depth, crack initiation location and the deepest point of the crack can be read from this figure. Based on this ACPD data record, the crack growth at the deepest point can be drawn out. This crack growth curve gives the crack developing procedure in the chord thickness, including the crack initiation phase and crack propagation phase. Then six-order polynomial curve fittings are then applied to the crack growth data to obtain smoothed profiles of surface cracks so that the “deepest points” (i.e. the sections with maximum a') were identified. Further analyses (polynomial fittings and differentiation) of the crack penetration data at the deepest points are employed to identify different crack growth phases and the crack penetration rate (da'/dN). A summary of the final results of the fatigue tests is listed in Table 4.2. For the analysis of test results, the following graphs are plotted:

- (1) Crack propagation and penetration profiles: graphs of a' against l (Figures 4.14, 4.18, 4.24 and 4.29 for Specimens I-IV respectively).
- (2) Crack penetration curve at the deepest point: graphs of a' against N at the deepest point (Figures 4.15, 4.19, 4.26 and 4.30 for Specimens I-IV respectively).
- (3) Crack penetration rate at the deepest point: graphs of da'/dN against a'/t_0 at the deepest point (Figures 4.16, 4.20, 4.27 and 4.31 for Specimens I-IV respectively).

- (4) Crack propagation curves: graphs of l against a' at the deepest point (Figures 4.17, 4.21, 4.28 and 4.32 for Specimen I-IV respectively).

During the tests, some phenomena were found, such as two crack appearance in Specimen II (Figure 4.23), crack branching out in Specimens III and IV (Figure 4.25). The plane views of the crack location are all shown out in Figures 4.7 to 4.10.

4.6.2 Fatigue Test Results of Specimen I

For Specimen I, the combined loads applied generate a distinct peak HSS near CP3 (Figure 4.5). A surface crack is initiated at near SI-P19. However, the deepest section gradually shifts to a section near SI-P20 and remains there throughout the test (Figure 4.14). Initially, the penetration rate is slow and the initiation phase is about 25% (10k cycles) of N_f , the total numbers of cycles applied (Figures 4.15 and 4.16). After the initiation phase, the crack develops steadily with a' increased proportionally with N , the number of load cycles applied. This phenomenon is somehow different from the cases of CHS T-joints (Chiew et. al 2004) in which after the initiation phase, the surface crack penetrates into the chord rapidly. Figure 4.16 also indicates only a mid increase in the penetration rate near the end of the test. For the propagation of the crack, as predicted from the SCF results, the crack propagates towards CP4 (Figure 4.5) as its depth increase (Figure 4.17) with a final ratio of $a'/l=0.08$ (Table 4.2).

4.6.3 Fatigue Test Results of Specimen II

Specimen II is designed to test the prediction that even without any OPB load, the crack will still propagate in a direction parallel to the chord wall. A relatively small amount of OPB load is applied (Figure 4.2) so that the peak HSS near CP4 is only about 10% higher than that at CP1 (Figure 4.5). A crack is initiated at near SII-P17 (Figure 4.18) and remains as the deepest point until the end of the test. When compared with Specimen I, a shorter initiation phase corresponding to 10% of N_f is recorded

(Figure 4.19). The penetration rate then increases until $a'/t_0 \approx 0.6$ ($a' \approx 10\text{mm}$) after which it drops sharply (Figures 4.19 and 4.20). Reviewing the records of the test shows that this was due to the deflection at the brace end of the specimen. When $a' \approx 10\text{mm}$, the crack was considerably long ($l \approx 150\text{mm}$, Figure 4.21) and caused a deflection $\delta \approx 10\text{mm}$ at the end brace end (Figure 4.22). The AX load thus produced a secondary OPB load equivalent to $0.01\text{m} \times 76\text{kN} = 0.76\text{kNm}$ which cancelled out part ($\approx 85\%$) of the OPB load ($-0.413\text{kN} \times 2.165\text{m} = -0.894\text{kNm}$) applied. As a result, the HSS at CP4 was effectively reduced which resulted in a drop in the penetration rate there. With further load cycles applied, this deflection eventually cancelled out almost all of the OPB load and a second crack was developed at CP1 (Figure 4.23). However, despite the absence of OPB load and the appearance of the second crack, the first crack still propagated along the line CP4-CP3 with a final ratio of $a'/l = 0.05$ (Table 4.2).

4.6.4 Fatigue Test Results of Specimen III

For Specimen III, no AX load was applied (Figure 4.3) and the nearly identical IPB and OPB loads generated peak HSS near CP4 (Figure 4.5). As a result, a crack is initiated near SIII-P19 and quickly shifts to S3-P18 and remained there (Figure 4.24). The initiation phase accounts for about 5% of N_f with a sharp increase in penetration rate (Figures 4.26 and 4.27). After that the penetration rate is dropped but eventually increased again when $a' \approx 8\text{mm}$. Similar to Specimens I and II, the crack mainly propagates along the line CP4-CP3 (Figure 4.9) and with little advancement towards CP1. However, for this specimen, when $l \approx 120\text{mm}$ ($a' \approx 15\text{mm}$ at the deepest section), the crack tip near the probe SIII-P23 branched out from the weld toe (Figures 4.9 and 4.24). Nevertheless, the crack propagation rate is slow along the branch with only about 10-15mm advancement while along the chord wall direction, more than 85mm advancement is recorded in subsequent cycles with a final ratio of $a'/l = 0.06$ (Table 4.2).

4.6.5 Fatigue Test Results of Specimen IV

For Specimen IV, equal magnitude of IPB and OPB loads are used and the AX load applied generates tensile stress around the *whole* intersection (Figure 4.5). The peak HSS occurs at CP3 and the crack is initiated near probe SIV-P10 (Figure 4.29). The crack then penetrates into the chord steadily (Figure 30) with section SIV-P10 remained the deepest point and a rapid increase of penetration rate is also observed near the end of the test. The initiation phase at SIV-P10 is short (2% of N_f). As the crack continues to grow, the surface crack turned to a TTC at near SIV-P22 (Figure 4.29). Despite the fact that the crack penetrates the chord at SIV-P22, due to the differences in crack surface profile at different sections, it does not correspond to a maximum value of a' . The initiation phase at SIV-P22 is about 75% of N_f , since it takes many load cycles for the crack to propagate to that location. However, after the initiation phase, the penetration rate at there is very high (Figures 4.30 and 4.31). As the whole connection is under tensile stress, near the end of the test, a second crack appears at SIV-P29 ($\approx 75\%$ of N_f) and branches are eventually formed at both ends of the crack at SIV-P29 ($\approx 85\%$ of N_f) and SIV-P6 ($>85\%$ of N_f) with even higher penetration rates (Figure 4.31). However, by the time the second crack and the branches are formed, the ratio of a'/t_0 at SIV-10 was already ≥ 1.0 . As expected, despite the present of tensile stress around the whole intersection, the crack again mainly propagates along the chord wall direction (Figure 4.10) with a final ratio of $a/l=0.05$ (Figure 4.32 and Table 4.2) and brace end deflection $\delta=16\text{mm}$.

4.6.6 Fatigue Life of the four specimens

In the previous studies of CHS joints (Bowness and Lee, 1995 and Chiew et al, 2004), the fatigue life of a joint is normally defined as N_3 , the number of cycles when TTC are formed [UK DEn 1993]. However, from the test results it is found that, for a RHS joint, extensive crack propagation ($l \approx$ brace width) and penetration ($a' > 1$) can occur well before a TTC is formed. In fact, for the four specimens tested, TTC was formed only in Specimen IV after more than two millions of load cycles. Hence, it is suggested that the fatigue life of a RHS joint should be defined as N_a , the number of load cycles when

$a'/t_0=1.0$ is reached at a section of the crack. For the four specimens tested, $N_{a'}$ is about 55% to 85% of N_f as shown in Table 4.2. In Figure 4.33, the S-N curve (Zhao et al, 2000) corresponding to the specimen chord thickness (16mm) is plotted against the values of $N_{a'}$ and N_f obtained from the tests. From Figure 4.33, it appears that the S-N curve is still conservative even when the more stringent criterion of $N_{a'}$ is adopted.

4.7 CRACK GEOMETRY MEASUREMENT

The fatigue test results and ACPD readings strongly indicates that, for a RHS joint, the shape of the crack surface cannot be described by a constant θ_s . In order to obtain more details of the crack shape, the cracked joints are cut out from the specimen and split into two parts, namely, the chord face part and the brace wall part. Plane and sections views of the crack surfaces exposed are shown in Figure 4.34 and 4.35 respectively. Figure 4.34 shows that the geometry of the crack surface changes along brace-chord intersection and is highly unsymmetrical with respect to the deepest point. Furthermore, Figure 4.35 shows that for a given section, the angle θ_s increases as the crack penetration into the chord wall. In order to gather further details for the development of an appropriate numerical model, a simple clay molding procedure was employed to measure the section profile (shape of curve **O'd'** in Figure 4.13) of the crack and the variations of θ_s at different sections. This molding procedure consists of the following steps:

- (1) After the crack surface is cleaned properly, soft fine clay is applied to the brace wall part (Figure 4.36a).
- (2) The clay mold is then removed when it was half-dry. As a result, the shape of the crack is imprinted onto the clay mold (Figures 4.36b and c).
- (3) The clay mold is allowed to dry completely. Slices of clay are trimmed off at 10mm intervals in directions perpendicular to the weld toe (Figure 4.36d).
- (4) For each section, the profile (curve **O'd'** in Figure 4.13) is transferred to a tracing paper (Figure 4.36e).

- (5) The profiles are digitalized by scanning and processed by the AutoCAD software and the variation of θ_s with y' is measured (Figure 4.36f).

Note that in step (5), in order to determine θ_s for a typical point $\mathbf{q}'$ on the profile (Figure 4.13), the coordinates (x', z') of $\mathbf{q}'$, which can be determined with error $\leq 1\text{mm}$, are measured. Since $\tan(\theta_s) = x'/z'$, $e(\theta_s)$, the error of θ_s will decrease as z' increases. By using the above procedure, it is estimated that $e(\theta_s) \leq 4^\circ$ when $z' = t_0 = 16\text{mm}$. A local coordinate s' is defined to describe the variation of θ_s at different sections of the crack (Figure 4.37). s' is measured from M, the intersection of the weld toe with the middle of the brace wall. For a typical point Q on the weld toe, s' is defined as the length of the curve MQ such that at points B and D, $s' = b/2$ and $(b + \pi r)/2$ respectively (Figure 4.37). The variations of θ_s along the surface crack corresponding to different a/t_0 levels for Specimens I to IV are plotted in Figures 4.38 to 4.41 respectively. From these figures, it can be observed that:

- (1) In general, θ_s increases as a/t_0 increases at all sections for all the specimens.
- (2) The rate of change of θ_s seems to be higher at the corners (between the curve BD) than between the line MB. Hence, the final range of θ_s is larger at the corners.
- (3) The initial values of θ_s fluctuate a lot when $a/t_0 < 0.2$ and smoothed out as a/t_0 increases. This can be explained by the higher measurement error when y' is small.
- (4) When $a/t_0 > 0.2$, values of θ_s at the corner(s) are higher than that between the line MB.
- (5) In all the specimens, peaks values of θ_s occurred at a section close to the quarter point C_1 of the corner of the chord (Figure 4.37).

From Figures 4.38 to 4.41, as expected, the variation of θ_s is depends on the welding details and loading applied. Nevertheless, it is found that the information obtained could be used to construct a reasonably good geometrical model for the prediction of fatigue life of a cracked RHS joint (which will be described in Chapter 5).

Except for the crack surface profiles, another parameter measured is the final crack shape which is the length of curve $\mathbf{O'd'}$, a' , in Figure 4.13. Every a' of each cut-section in Specimen I, II and III, is measured manually, which are then compared with ACPD recording data in Figure 4.42. It can be seen that the results of measurements of three points, namely, two crack tips and the deepest point, match well with the ACPD recordings. It is shown that ACPD technique is stable and suitable for the monitoring of the crack propagation in SSHS joints. Another observation is that not all the cracks developed in semi-ellipse shape as found in circle hollow section joints (Chiew et al, 2004). Two quarter-ellipses can be used as the representation of the crack shape to ensure that the spatial derivative (slop) of the crack shape at the deepest point is zero, as compared in Figure 4.42.

4.8 FATIGUE PERFORMANCE COMPARISONS BETWEEN CHS AND SHS

The tests carried out above showed that the fatigue performance of a SSHS joint is very different with that of a CHS joint. Huang (2002) and Shao (2004) had carried out full-size testing on CHS T-joints and K-joints respectively. Three same geometrical dimension CHS T-joint specimens were tested subjected to different loads by Huang (2002). The details of the experimental study on the fatigue performance of CHS T-joints also can be found in Chiew et al (2004). Two CHS K-joints with different brace angle were tested by Shao (2004). From the comparisons between the crack propagation in these two cross-section hollow section joints, some observations may be drawn.

1. Crack shape is basically symmetric in CHS joints no matter where the crack initiates at beginning, i.e. the crown, the saddle or between the crown and the saddle (Chiew et al, 2004). Semi-ellipse can be the representation of the crack shapes. However, crack shapes in square-to-square tubular joints depend on the stage of the crack propagation. At early stage, the crack shape is almost

symmetric, but at last stage, the crack shape is unsymmetrical (please refer to Figures 4.14, 4.18, 4.24 and 4.29). In this condition, two quarter-ellipses are used as the representation of the crack shape (Figure 4.42).

2. The crack surface curves mostly under the weld toe in SSHS joints especially around the corner (please refer to Figure 4.35). The crack surface in SHS joints is twist (please refer to Figure 4.34). That means that the crack is a 3-dimensional curved surface. The curvature of this surface varies along the weld toe and also varies in the depth direction. It is unrealistic to describe the crack surface in SSHS joints using a straight line or a single-variable function. This will be a challenging job in the modelling of the crack surface. However, in CHS joints, Huang (2002) and Shao (2004) found that the crack surface was almost a straight line when the crack depth was less than $0.8T$, where T is the chord thickness. When crack depth exceeded $0.8T$, the crack surface curved slightly. Hence, in their FE model, a straight line was used to represent the crack surface. This will greatly reduce the difficulty of the simulation of the cracks in a CHS joints.
3. In the fatigue tests in SSHS joints, it was found that the cracks in both Specimen III and Specimen IV deviated from the weld toe (please refer to Figures 4.24 and 4.29). In fatigue tests of CHS both T-joints and K-joints in Huang and Shao's work, no cracks were found going out off weld toe.
4. From the tests, it was found that when a crack initiates and develops at one of the four corners of a square-to-square connection, the external load will be re-distributed. However, this re-distribution is rather localized within the corner. Such stress re-distribution will virtually not affect other corners. That may give the reason why a second crack appears at another corner after the first one (please refer to figures 4.8 and 4.23). However, in CHS, the load carrying area converges at the crack zone after one crack initiation. Hence the possibility of the appearance of a second crack is very small.
5. The fatigue life of one joint includes crack initiation phase and crack propagation phase. The crack growth curve showed that the crack initiation

phase account for a small part in the whole fatigue life of SSHS joints (please refer to figures 4.15, 4.19, 4.26 and 4.30). Except for Specimen I, the crack initiation phase account for less than 10% in specimen II, III and IV. However, in CHS joints, both T-joint and K-joint showed that the crack initiation phase account for more than 25% of the fatigue life. The crack propagation in SSHS joints is quite smooth while in CHS joints, the crack propagation accelerates quickly.

4.9 CONCLUDING REMARKS

In this experimental study, four full-size SSHS joints are subjected to both static and fatigue tests. The static results obtained indicate that peak HSS always occurs at the corner point of the joint. The measured SCFs are different from those predicted by the existing guideline. It is also shown that the superposition method can be used to predict the SCF distribution when the joint is subjected to combined loads. Furthermore, when compared with CHS joints, a higher stress-strain conversion factor of 1.2 should be used for the case of RHS joints.

The fatigue tests demonstrated that under cyclic loading, cracks initiate at the corners of the brace chord intersections and propagate in a direction parallel to the chord wall, even without any OPB load. Results obtained by the ACPD technique and post-failure measurements show that the shape of surface cracks of a RHS joint is more complicated than that of a CHS joint. The crack is unsymmetrical with respect to the deepest point and the crack profile at any cross section is a curve. The crack growth rate at the deepest point may not increase monotonically with the number of load cycles applied. Significantly long and advanced crack could appear well before a TTC is formed. Furthermore, when near the end of the fatigue life, secondary cracks and branching, which seldom appear in CHS joint, are observed in the tests. Based on the test results, rather than adopting the commonly used criterion N_3 , it is suggested that the fatigue life of a RHS joint should be considered as N_a , the number of load cycles when

the curved crack depth is equal to the thickness of the chord. Nevertheless, experimental results indicate that the current S-N curve used for design is still conservative even when the more stringent criterion of N_a is adopted.

The results obtained in this study strongly suggested that any of the existing geometrical model developed for cracked CHS joints could not be applied for the numerical modelling of a cracked RHS joint. However, the experimental results obtained could be employed for the development of a reasonable model for this type of joint. This provides rare information for the further numerical modelling and FEM study.

Figure 4.1 Cyclic load applied to Specimen I

Figure 4.2 Cyclic load applied to Specimen II

Figure 4.3 Cyclic load applied to Specimen III

Figure 4.4 Cyclic load applied to Specimen IV

Figure 4.5 Hot spot stress distribution on chord

Figure 4.6 An ACPD theory and notation

Figure 4.7 A plan view showing the probe location in Specimen I

Figure 4.8 A plan view showing the probe location in Specimen II

Figure 4.9 A plan view showing the probe location in Specimen III

Figure 4.10 A plan view showing the probe location in Specimen IV

Figure 4.11 A close-up view of ACPD probe sitting

Figure 4.12 ACPD probes connected to U10 by cables

Figure 4.13 Definitions of geometrical parameters of a surface crack

Figure 4.14 Curved crack depth (a') history for Specimen I

Figure 4.15 Crack penetration curve at section SI-P20

Figure 4.16 Crack penetration rate (da'/dN) at section SI-P20

Figure 4.17 A graph of crack length l against curved crack depth a' at section SI-P20

Figure 4.18 Curved crack depth (a') history for Specimen II

Figure 4.19 Crack penetration curve at section SII-P17

Figure 4.20 Crack penetration rate (da'/dN) at section SII-P17

Figure 4.21 A graph of crack length l against curved crack depth a' at section SII-P17

Figure 4.22 Eccentricity caused by the dropdown of the brace end

Figure 4.23 Two cracks in Specimens II

Figure 4.24 Curved crack depth (a') history for Specimen III

Figure 4.25 Crack branching in Specimen III

Figure 4.26 Crack penetration curve at section SIII-P18

Figure 4.27 Crack penetration rate (da'/dN) at section SIII-P18

Figure 4.28 A graph of crack length l against curved crack depth a' at section SIII-P18

Figure 4.29 Curved crack depth (a') history for Specimen IV

Figure 4.30 Crack penetration curve at section SIV-P10, SIV-P22 and SIV-P29

Figure 4.31 Crack penetration rate (da'/dN) at section SIV-P10, SIV-P22 and SIV-P29

Figure 4.32 A graph of crack length l against curved crack depth a' at section SIV-P10

(Note: Values of l for $a' > 18$ mm are not recorded as the crack propagated outside range of ACPD probes)

Figure 4.33 Fatigue test results comparing with S-N curve data

Figure 4.34 Plane views of the chord face parts

Figure 4.35 Section views of the brace wall parts

(a) Fine soft clay is applied

(b) Dried clay mold was removed

(c) Crack surface imprinting on clay mold

(d) Sections of the mold are prepared

(e) Transfer of cross sections to tracing papers

(f) Crack shape marked on paper scanning

Figure 4.36 Procedure to measure the geometry of a surface crack

Figure 4.37 Local coordinate s' for the measurement of θ_s

Figure 4.38 Measured values of θ_s , Specimen I

Figure 4.39 Measured values of θ_s , Specimen II

Figure 4.40 Measurement values of θ_s , Specimen III

Figure 4.41 Measurement values of θ_s , Specimen IV

(a) Final crack shape in Specimen I

(b) Final crack shape in Specimen II

(c) Final crack shape in Specimen III

Figure 4.42 Final crack shape in Specimen I, II and III

Table 4.1 Actual nominal stress caused by cyclic loads

Load type	Specimen I		Specimen II		Specimen III		Specimen IV	
	σ_{n-max} (MPa)	σ_{n-min} (MPa)	σ_{n-max} (MPa)	σ_{n-min} (MPa)	σ_{n-max} (MPa)	σ_{n-min} (MPa)	σ_{n-max} (MPa)	σ_{n-min} (MPa)
AX	8.90	0.0	6.35	0.0	0.0	0.0	9.1	0.0
IPB	7.83	0.0	0.0	-10.80	0.0	-19.0	5.68	0.0
OPB	0.0	-6.63	0.0	-1.40	0.0	-16.0	0.0	-6.56
Note: Positive loads are tension loads, and negative loads are compression loads applied by actuators as shown in Figure 3.2 in Chapter 3 AX – axial, IPB – in plane bending, OPB – out of plane bending								

Table 4.2 Summary of fatigue test final results

Results	Specimen I	Specimen II	Specimen III	Specimen IV
Total no. of cycle applied N_f	407517	446508	899289	2459643
No. of cycle when $a'/t_0=1.0, N_{a'}$	340500	277250	493370	2044700
Final l (to the nearest 5mm)	150	255	230	290
Final a' (mm)	20.03	20.53	30.14	29.29
Final a/t_0	0.75	0.79	0.89	1.0

CHAPTER 5

GEOMETRICAL MODELLING AND FEM MESH GENERATION FOR SSHS T-JOINTS

5.1 INTRODUCTION

Although large-scale steel joint fatigue tests can provide actual information about the fatigue performance of the hollow section joint, they are time consuming and very expensive to carry out. Due to the limitations of the testing facilities, it is also difficult to implement all the possible load cases to the test specimens. The finite element method (FEM) provides an inexpensive, fast and flexible way for fatigue study of the hollow section joints. Hence, in the past few decades, many researchers have put their effort into this modelling of the hollow section joint with and without cracks. Haswell and Hopkins (1991) also concluded that FEM is the technique with the most potential to accurately model geometry, loading and structural restraint of the hollow section joints after reviewing some fracture mechanics models for the tubular joints. However, it has been found that it is not a simple matter using finite elements, especially the 3D elements, to generate good quality meshes for most cracked joints because of the complex geometry of intersections between members, and the difficulty in modelling welds and cracks (Cao et al, 1998).

During the past few decades, some natural techniques that can accurately model the hollow section joints with or without cracks were developed. These techniques are used in circular hollow section (CHS) joints. One of them is developed by Bowness and Lee (1995b), which can convert the 3D T-butt mesh model to cracked CHS joint mesh model. In the program, the 3D cracked T-butt mesh is generated first, and then it is

mapped into a CHS joint intersection. The details of modelling sequence can be found in Bowness and Lee (1995b). They used this method to analyze some cracked CHS joints and got some good results. However, the problem of this method is that only one quarter of joint can be modelled, and the crack can only be generated at the crown or saddle. This also implies that in the full model, the cracks are on both saddles and crowns instead of just one based on this assumption. This should have great influence on the results when the crack at the intersection of the joints is long, although Bowness and Lee (1995a) used this symmetric method to analyze a joint with a crack of 25% length of the intersection and showed that the effect on the results is small.

Another technique is developed by Cao et al (1998) who changed the complicated 3D mesh generation procedure for the intersection of CHS joint to a procedure of mesh generation in a flat plate. In this program, a crack element group around a surface crack front was generated first. Then the whole group was inserted into the flat plate mesh, and twenty nodes were created along the crack surfaces and the crack elements were connected to the adjacent elements. After mapping, the cracked CHS joint mesh model was produced. The advantages of this method are that the mesh can be modified easily, different crack tip models can be used, and the location and the length of the crack can be changed. However, inserting crack elements into the plane mesh and connecting them to the adjacent elements still seems to be a complicated task. At the same time, the fatigue analysis results produced by this technique are very little in the publications and not easy to be validated.

Based on the geometrical analyses of the CHS T&Y-joints with weld and crack, Wong (2001) also proposed a method at Nanyang Technological University which is similar to that of Cao et al (1998). This method also changed the complicated 3D mesh generation procedure for the intersection of CHS joint to a procedure of mesh generation in a flat plate. The weld and the crack were also modelled in the flat plate and then mapped back to the 3D mesh of the cracked CHS T&Y-joint. Then a primal mesh generator based on this method was developed and used to generate some

cracked and uncracked CHS T&Y-joints for analysis (Lee et al, 1999 and Lie et al, 2000). However, it was found that this mesh generator had some shortcomings. The major one is that the mesh topology is fixed and cannot change with the different situation of the cracked joints, for example the intersection of the joint is divided to 32 segments and has no relation to the geometry of the joints. That may not be suitable to guarantee the mesh quality around the intersection of joints and that of the crack. Later, the modelling technique was refined by Huang (2002) who developed a more flexible mesh generator. The intersection of the joints can be divided into arbitrary segments as needed and contact surface was also included.

Although so many techniques have already been developed, most of them simplified the crack surface with a straight line perpendicular to the chord surface (Rhee et al, 1991, Cao et al, 1997, Lee et al, 1999 and Lie et al, 2000). This is because of the extreme difficulty in modelling the actual situation of a crack curving around the hollow section joint intersection as well as under the weld toe. Bowness and Lee (1998) studied the fatigue crack surface under the weld toe in the CHS joint and found that the stress intensity factors (SIFs) derived from a straight perpendicular crack is not significantly different with that for a crack with the crack surface expressed by a polynomial function. It means that the crack surface under the weld toe can be simplified to a straight perpendicular line in CHS joints. However, these experiences cannot be used in the rectangular hollow section (RHS) joints directly due to the geometric difference. The fatigue tests in Chapter 3 and 4 have already shown that the fatigue performance of the square-to-square hollow section (SSHS) joints much differs from that of CHS joints. The crack in square hollow section (SSHS) joints has a twist surface which not only curves under weld toe, but also changes with the location around weld toe. Furthermore, up to today there is little information about the modelling of the RHS joints with cracks that can be located at any position. Burdekin and Laham (1993) carried out simulation of RHS joints with a crack, but the crack was located at the straight line of the intersection. Static tests in Chapter 3 have shown that the peak hot spot stress (HSS) site that is expected to be the location of crack initiation

is always located at the corner of the joints. Hence, a new technique that can accurately model the SSHS joints with cracks is needed to perform the fatigue analysis.

Arising from above discussion, a systematic geometrical modelling procedure for a general SSHS T-joint with or without cracks is proposed in this study. The welding details along the brace-chord intersection are compatible with the American Welding Society (AWS) standard (2000). The crack surface is modeled as an unsymmetrical 3D curved surface with deepest point located at the corner of the brace-chord intersection. The crack shape is modeled as a piecewise unsymmetrical bi-elliptical curve. Subsequently, a mesh generator is developed. Such a model can contain single or multiple cracks and the crack can be any length and located at any positions along the brace-chord intersections. The convergence and the validation of the mesh models are evaluated.

5.2 GEOMETRICAL MODELLING OF SSHS T-JOINTS

A valid, consistent and realistic geometrical model for the surface crack is a pre-requisite for the development of a valid numerical model of a cracked square-to-square hollow section (SSHS) joint. The following three steps are employed to develop the geometrical model of the surface cracks:

- (1) Definition of the weld toe;
- (2) Definition of the crack surface;
- (3) Definition of the crack front;

5.2.1 Definition of the Weld Toe

The basic configuration for a SSHS T-joint is shown in Figure 5.1. Due to the simple sectional property of SHSS, the brace-chord intersection curve of a SSHS T-joint is relatively simple as compared with CHS joints and it only consists of four straight line

segments and four corners (CP1-CP4). The global coordinate system (x - y - z) of the joint is defined with the origin O' located at the intersecting point between brace centroid and the chord face (Figure 5.1). Without losing generality, one may consider the corner CP3 for the definition of the weld toe (Figure 5.2). As shown in Figure 5.2, the arc LH defines the brace corner at CP3 and sustains an angle $\omega \leq \pi/2$ at F such that $|FL| = |FH| = r_b$. Furthermore,

$$\begin{cases} \angle HFQ = \pi/4 - \omega/2 \\ |FQ| = r_b \cos(\pi/4 - \omega/2) \\ |HQ| = r_b \sin(\pi/4 - \omega/2) \end{cases} \quad (5.1)$$

Hence, the co-ordinates of F, (x_F, y_F, z_F) , are given by

$$\begin{cases} x_F = -b_1/2 + r_b \cos(\pi/4 - \omega/2) \\ y_F = -b_1/2 + r_b \cos(\pi/4 - \omega/2) \\ z_F = 0 \end{cases} \quad (5.2)$$

If the welding thickness is constant along the whole brace-chord intersection and is equal to W , then the arc B'D' (Figure 5.2) is the weld toe corresponding to the arc LH. However, in practice, the actual weld toe is normally a smooth curve which sustains a right angle around the corner. Hence, in this study, a full right angle arc BD with centre at G is used instead such that the line GHB is perpendicular to HQ. From Figure 5.2,

$$\begin{cases} |GH| = |LG| = |FQ| - |HQ| = r_b (\cos(\pi/4 - \omega/2) - \sin(\pi/4 - \omega/2)) = \sqrt{2}r_b \sin(\omega/2) \\ |GB| = |GH| + |HB| = \sqrt{2}r_b \sin(\omega/2) + W = r_w \end{cases} \quad (5.3)$$

The co-ordinates of G, (x_G, y_G, z_G) , are then given by

$$\begin{cases} x_G = -b_1/2 + r_b(\cos(\pi/4 - \omega/2) - \sin(\pi/4 - \omega/2)) = -b_1/2 + \sqrt{2}r_b \sin(\omega/2) \\ y_G = -b_1/2 + r_b(\cos(\pi/4 - \omega/2) - \sin(\pi/4 - \omega/2)) = -b_1/2 + \sqrt{2}r_b \sin(\omega/2) \\ z_G = 0 \end{cases} \quad (5.4)$$

Finally, from Eqns. (5.3) and (5.4), equations of the weld toe for the quarter M₃-CP3-M₄ can be summarized as

$$\begin{aligned} \text{From M}_3 \text{ to D:} \quad & y + b_1/2 + W = z = 0 \\ \text{For arc B to D:} \quad & (x - x_G)^2 + (y - y_G)^2 - (r_w)^2 = z = 0 \\ \text{From B to M}_4: \quad & x + b_1/2 + W = z = 0 \end{aligned} \quad (5.5)$$

Obviously, equations for other quarters of the joint can be obtained in a similar manner. It should be noted that Eqns. (5.1) to (5.5) can be extended to the cases of RHS T/K joints with modest modifications and by adopting suitable values of W and ω .

In AWS (2000), the minimum value of W is specified for various types of joints, as attached in Appendix A. For hot finished square hollow section, EN10210 (1997) gives out the calculated values for r_b , which is $1.5t_1$. The value of C_1 and C_2 should be measured (Appendix B). Then the angle ω can be calculated

$$\omega = 2 \cdot \arcsin(\sqrt{C_1^2 + C_2^2} / (2 \cdot r_b)) \quad (5.6)$$

In the experimental studies, the values of C_1 and C_2 are measured, $C_1 = C_2 = t_1$. Hence, $\omega = \pi/3$ is adopted.

5.2.2 Definition of Crack Surface Profile

Figure 5.3a shows a typical surface crack extends from $\mathbf{p}_1$ to $\mathbf{p}_2$ at the corner CP4. The local coordinate s' is defined such that at point M, $s'=0$; at $\mathbf{p}_1$ and $\mathbf{p}_2$, $s'(\mathbf{p}_1)=s'_1$, $s'(\mathbf{p}_2)=s'_2$ and $s'_2 - s'_1 = l$, the *length* of the surface crack. From the results obtained in fatigue tests (Chapter 4), the crack profile, $\mathbf{O'd'}$ for the section $I'-I'$ perpendicular to the weld toe at $\mathbf{O'}=(x_{O'}, y_{O'}, 0)$ is in general a curve (Figure 5.3b). A local coordinate system $\mathbf{x'-y'-z'}$ is defined with origin at $\mathbf{O'}$ in such a way that the $\mathbf{x'}$ and the $\mathbf{y'}$ axes are, respectively, perpendicular and tangential to the weld toe at $\mathbf{O'}$. $\mathbf{d'}$ is the deepest point of the crack such that a' and a are, respectively, the *curved* and *projected* lengths (to the $\mathbf{z'}$ axis) of $\mathbf{O'd'}$. The angle between the global $\mathbf{x}$ and local $\mathbf{x'}$ axes is equal to $\pi-\phi(s')$.

From the experimental measurements obtained in fatigue tests (Chapter 4), it is found that for a typical point $\mathbf{q'}$ on the crack surface, the angle $\theta_s(z')$ increases as z' increases and almost converges to a constant value near the bottom of the chord. Hence, in the present study, the equation which describes the geometry of the surface crack will be devised from the following assumptions:

- (1) The crack profile will be modeled as a curve for $0 \leq z' \leq z'_c$ and a straight line for $z'_c \leq z' \leq t_0$. The value z'_c is known as the *critical* depth for the crack and a value of $z'_c = 0.8t_0$ is adopted in this study.
- (2) For $0 \leq z' \leq z'_c$, $\theta_s(z')$ will increase linearly with z' from $\theta_s(0)$ to $\theta_s(z'_c)$ (Figure 5.3c) such that

$$\theta_s(z') = \theta_s(0) + [\theta_s(z'_c) - \theta_s(0)]z'/t_0, \quad 0 \leq \theta_s(0) \leq \theta_s(z'_c) < \pi/2 \quad (5.7)$$

and when $z' = z'_c$, $x' = z'_c \tan(\theta_s(z'_c))$.

(3) For $z'_c \leq z' \leq t_0$, the crack profile will be a straight line with slope equal to the gradient of the curve at $z' = z'_c$. That is, in Figure 5.3c, the angle ϕ_c is given by

$$\tan(\phi_c) = \left. \frac{dz'}{dx'} \right|_{z' = z'_c} \quad (5.8)$$

By using the above assumptions, equations for the crack profile with respect to the x' - y' - z' coordinate system can be obtained as

$$\text{For } 0 \leq z' \leq z'_c: \quad x' = z' \tan(\theta_s(0)) + [(\theta_s(z'_c) - \theta_s(0))z'/t_0] \quad (5.9a)$$

$$\text{For } z'_c \leq z' \leq t_0: \quad \frac{z' - z'_c}{x' - z'_c \tan(\theta_s(z'_c))} = \tan(\phi_c) = \left. \frac{dz'}{dx'} \right|_{z' = z'_c} \quad (5.9b)$$

From Eqn. (5.9a),

$$\tan(\phi_c) = \left. \frac{dz'}{dx'} \right|_{z' = z'_c} = \left[\left. \frac{dx'}{dz'} \right|_{z' = z'_c} \right]^{-1} = \frac{\cos^2(\theta_s(z'_c))}{\sin(\theta_s(z'_c))\cos(\theta_s(z'_c)) + (\theta_s(z'_c) - \theta_s(0))} \quad (5.10)$$

and from Eqns. (5.7) and (5.10), since $\theta_s(0) \leq \theta_s(z'_c) < \pi/2$, then $\tan(\phi_c) > 0$ and the crack surface will reach the bottom of the chord ($z' = t_0$) when

$$x' = c \tan(\phi_c)(t_0 - z'_c) + z'_c \tan(\theta_s(z'_c)) = t_0 \tan(\theta_s(z'_c)) + \frac{(t_0 - z'_c)(\theta_s(z'_c) - \theta_s(0))}{\cos^2(\theta_s(z'_c))} \quad (5.11)$$

From the measurements done in fatigue tests after the tests are finished (Chapter 4), it was found that the angles $\theta_s(0)$ and $\theta_s(z'_c)$ will vary along the crack length (s') direction. Actual measurements indicate that $\theta_s(0)$ usually starts from a small initial

value $\theta_s^i(0)$ at M and increases almost linearly until the point B ($s' = L_w = b_l/2 - \sqrt{2}r_b \sin(\omega/2)$). After that $\theta_s(0)$ varies more rapidly and attends a maximum value $\theta_s^{max}(0)$ at the point P ($s' = L_w + \pi r_w/8$) and then decreases until the point D ($s' = L_w + \pi r_w/2$). In fact, fatigue tests showed that the extension of the surface crack is often limited with the range of $0 \leq s' \leq L_w + \pi r_w/2$, that is from point M to point D (Figure 5.4a). The variation of $\theta_s(z'_c)$ with s' follows a similar trend and for $z'_c = 0.8t_0$, it starts with an initial value of $\theta_s^i(z'_c)$ at M and attends a maximum value of $\theta_s^{max}(z'_c)$ at point P. Based on the above observation, in this study, the variations of $\theta_s(0)$ and $\theta_s(z'_c)$ will be modelled under the following conditions (Figure 5.4b):

- (1) Graphs of $\theta_s(0)$ and $\theta_s(z'_c)$ against s' will be a straight line for $0 \leq s' \leq L_w$ with initial values equal to $\theta_s^i(0)$ and $\theta_s^i(z'_c)$ ($z' = 0.8t_0$) respectively.
- (2) The variations will be modelled as a quadratic curve for $L_w \leq s' \leq L_w + \pi r_w/2$ with maximum values of $\theta_s^{max}(0)$ and $\theta_s^{max}(z'_c)$ at the point P ($s' = L_w + \pi r_w/8$).
- (3) The straight line and quadratic curve of $\theta_s(0)$ and $\theta_s(z'_c)$ are continuous and smooth at the point B.

From the above conditions, the variation of $\theta_s(0)$ with s' , $\theta_s(0, s')$, can be expressed by the following piecewise smooth equations:

$$\theta_s(0, s') = \begin{cases} \theta_s^i(0) + \frac{16(\theta_s^{max}(0) - \theta_s^i(0))}{16L_w + \pi r_w} s' & \text{for } 0 \leq s' \leq L_w \\ \theta_s^{max}(0) - \frac{(\theta_s^{max}(0) - \theta_s^i(0))}{\pi r_w (16L_w + \pi r_w)} (8(s' - L_w) - \pi r_w)^2 & \text{for } L_w \leq s' \leq L_w + \pi r_w/2 \end{cases} \quad (5.12)$$

Similarly, the variation of $\theta_s(z'_c)$ with s' , $\theta_s(z'_c, s')$ can be obtained by replacing the terms $\theta_s^i(0)$ and $\theta_s^{max}(0)$ in Eqn. (5.12) by $\theta_s^i(z'_c)$ and $\theta_s^{max}(z'_c)$ respectively and one can get the equation:

$$\theta_s(z'_c, s') = \begin{cases} \theta_s^i(z'_c) + \frac{16(\theta_s^{max}(z'_c) - \theta_s^i(z'_c))}{16L_w + \pi r_w} s' & \text{for } 0 \leq s' \leq L_w \\ \theta_s^{max}(z'_c) - \frac{(\theta_s^{max}(z'_c) - \theta_s^i(z'_c))}{\pi r_w (16L_w + \pi r_w)} (8(s' - L_w) - \pi r_w)^2 & \text{for } L_w \leq s' \leq L_w + \pi r_w / 2 \end{cases} \quad (5.13)$$

By combining Eqns. (5.9), (5.10) (5.12) and (5.13), the crack surface at any typical cross section with $y'=0$ can be expressed as

$$\text{For } 0 \leq z' \leq z'_c: \quad x' = z' \tan(\theta_s(0, s')) + [(\theta_s(z'_c, s') - \theta_s(0, s')) z' / t_0] \quad (5.14a)$$

$$\text{For } z'_c \leq z' \leq t_0: x' = \left[\tan(\theta_s(z'_c, s')) + \frac{(\theta_s(z'_c, s') - \theta_s(0, s'))}{\cos^2(\theta_s(z'_c, s'))} \right] (z' - z'_c) + z'_c \tan(\theta_s(z'_c, s')) \quad (5.14b)$$

Hence, by applying Eqn. (5.5), the values of $(x_{\theta'}, y_{\theta'})$ can be determined. Then according to the angle between the global x and x' axes, $\pi - \varphi(s')$ (Figure 5.1), the expression of the crack surface can be obtained,

$$\begin{bmatrix} x \\ y \\ z \end{bmatrix} = \begin{bmatrix} -\cos(\varphi(s')) & \sin(\varphi(s')) & 0 \\ -\sin(\varphi(s')) & -\cos(\varphi(s')) & 0 \\ 0 & 0 & 1 \end{bmatrix} \begin{bmatrix} x' \\ y' \\ z' \end{bmatrix} + \begin{bmatrix} x_{\theta'} \\ y_{\theta'} \\ 0 \end{bmatrix} \quad (5.15)$$

5.2.3 Definition of Crack Front

Actual measurement of crack front done after the fatigue test indicated that the crack front resembles an elliptical curve on a projected flat plane when the crack surface is opened up. Hence, in this study, the shape of the crack front will be modelled as a unsymmetrical bi-elliptical curve on the projected s' - z' plane of the form (Figure 5.5a)

$$\text{For } 0 \leq s' \leq s'_1 + l_d \text{ and } z' \geq 0: \quad \frac{(s' - (s'_1 + l_d))^2}{(l_d)^2} + \frac{(z')^2}{(a')^2} = 1 \quad (5.16a)$$

$$\text{For } s'_1 + l_d < s' \leq s'_2 \text{ and } z' \geq 0: \quad \frac{(s' - (s'_1 + l_d))^2}{(l - l_d)^2} + \frac{(z')^2}{(a')^2} = 1 \quad (5.16b)$$

In Eqn. (5.16), s'_i , $i=1, 2$ are the s' coordinates of the tip $\mathbf{p}_i$ of the surface crack and $l = s'_2 - s'_1$. Furthermore, the point with $s' = l_d + s'_1$ is corresponding to the deepest point $\mathbf{d}'$ (Figure 5.5b) on the crack front with curved and projected depth equal to a' and $z' = a$ respectively. Obviously, the bi-elliptical attends a maximum value of a' when $s' = s'_1 + l_d$ and becomes a single elliptical curve when $l_d = l/2$. Furthermore, the points on the crack front can be generated once the values of s'_i , l and l_d are known which can be measured from the experiments for validation of the model against the actual crack front shape. It is found in the four specimen tested, Eqn. 5.16 leads to a reasonable *conservative* model such that the modeled crack front is deeper than the measured one (please refer to Figure 4.42).

5.3 MESH GENERATION OF SSHS T-JOINTS WITH AND WITHOUT CRACKS

After the geometry of the welded joint and the surface crack are defined, FE meshes will be generated by a tailor-made mesh generator. An automatic mesh generation for producing the complete mesh of the model will be discussed in this section. It uses a

sub-zone technique whereby the entire structure is divided into distinct zones. In each zone, the mesh is generated separately. After the mesh of each zone has been completed, the mesh of the entire structure is generated by merging the meshes of all the zones.

It has been found from earlier study on the CHS T and Y-joints that sub-dividing the entire structure into different distinct zones can greatly simplify the mesh generation process (Huang, 2002 and Lie et al, 2003). For each zone, the finite element mesh is generated separately. When generating the mesh for a surface crack, an element block near the intersection is extracted and crack elements are generated separately. The number and location of these elements will depend on the length and the location of the crack of the welded joints. By using this approach, different meshes can be generated easily according to different requirements, such as refined mesh near the high stress gradient fields and coarse mesh far away from high stress gradient fields. Furthermore, it is much quicker to check and modify the mesh of this zone only, and most importantly, the elements' aspect ratio can be controlled accordingly.

This mesh generation will consist of two distinct steps, namely, (i) generation of mesh without surface crack for SCF study and (ii) generation of mesh with surface crack details for SIF study.

5.3.1 Choice of Element Type

Both shell and 3-D solid elements can be used to analyze uncracked tubular joints for obtaining the SCFs satisfactorily. However, it may not be accurate to model the welds using shell elements only as these elements ignore the shear stress in the thickness direction. Lee & Bowness (2001) had also concluded that the stress on the outer face of the chord is different from the stress on the inner face. The difference is due to the

bending effect. Hence, shell elements are not able to measure this difference. Lee & Bowness (2001) proposed a scale to define the difference, which is called the degree of bending (DOB). Hereby, Herion et al (1996) had proposed that 3-D quadratic solid elements are the best elements to model the tubular joints with welds. However, for a cracked tubular joint, it is not feasible to adopt only 3-D brick elements to model the entire structure because the geometry of the surface crack is quite complicated. The mesh quality of the area around the crack will influence the SIF results significantly. The crack front has to be modelled using different types of elements so as to produce good quality mesh. Bowness & Lee (1995b) and Cao et al (1997) had adopted two types of elements to simulate tubular joints with surface crack. 3-D prism singular elements are used to model the volume around the crack front, and 3-D hexahedral elements are used to model the zones far away from the crack front. However, it is found that the elements near the crack front are still badly distorted because the aspect ratios of some elements are very high. To overcome this deficiency, Wong (2000) and Huang (2002) had used five types of elements to generate the mesh of a T-joint and a Y-joint respectively.

Here, same five types of elements are used as Wong (2000) and Huang (2002) to generate the mesh of SSHS T-joint. The element types used are tabulated in Table 5.1. Hexahedral elements are used to simulate the field away from the crack front. Prism elements are used as the transition elements between the crack front and the field far away from the crack. Quarter-point elements (also called crack elements) are used to simulate the crack tip. For quarter-point elements, the mid-point is moved from the middle point to the one-fourth point near the crack tip. This type of elements has already been proved to be effective to simulate the crack tip singularity. Tetrahedron elements are adopted to link the crack elements and the adjacent elements. It can decrease the number of badly distorted elements. Finally, pyramid elements are used to fill the void of different elements linked together in the crack region.

5.3.2 Criterion of Mesh

From the previous studies (Wong, 2000; Huang, 2002 and Shao, 2004), it is thought that a good quality mesh generation should satisfy the following points:

1. The mesh should be of high quality near the region where high stress gradients exist.
2. Coarse mesh should be used in the region far away from the high stress gradients to save the calculating time and decrease the need for large memory requirements.
3. The mesh generation programs should be able to decide whether the parameters are reasonable.
4. The mesh generation programs should be able to adjust the aspect ratio of the elements automatically. This means the programs can identify the badly distorted elements and control the largest aspect ratio of these elements.
5. The possibility of ill-conditioned elements should be minimized.
6. The mesh of the structure should be symmetrical if the structure is symmetrical so as to simplify the modelling and reduces the computation time.

5.3.3 Implementation of Mesh Generation without Cracks

In the mesh generation, the T-joint model will be divided into different zones as shown in Figure 5.6. Each of these zones consists of different density elements. In the far field (Zones A, K1, K2, and K3), only one layer of 3D elements will be employed since the stress distribution along the thickness direction is nearly uniform in these regions. On the other hand, fine meshes with more than three layers of elements will be generated near the intersection (Zone CF, D1, D2 and E) of the joint so that the FE model will be able to capture the stress concentration near the intersection. Between the refined regions and the far field regions, transitions regions (Zones F1, F2 and F3) are used to reduce the number of layers of elements in the thickness direction from three to one. In

addition, Zones G1, G2, G3, H1, H2 and H3 are developed to reduce the number of elements around the brace-chord intersection. The mesh of each zone is generated individually and will be merged together to form the complete mesh (Figure 5.7). All the elements generated are linear elements because they can reduce the complexity of the generation process and thus bad distorted elements could be identified and modified easily. Besides, meshes in different zones could be merged and extracted easily for linear elements that consist of corner nodes only. It is noted that the mesh density can be adjusted by changing the element numbers around the intersection so as to be suitable for different joint geometry. As shown in Figure 5.8, two models with density mesh and coarse mesh are generated separately. Figure 5.8(a) is the model with 48 elements around the intersection (not lose in generality, 6 elements along M3-CP3, 4 elements around CP3, and 6 elements along CP3-M4, Figure 5.2) and Figure 5.8(b) is the model with 96 elements (12 elements along M3-CP3, 8 elements around CP3, and 12 elements along CP3-M4).

5.3.4 Implementation of Mesh Generation with Surface Cracks

The procedure to generate a mesh with surface crack is more complicated than the one without crack and can be summarized as the following steps:

- (i) A mesh without surface crack will be generated by the procedure described in above section (Section 5.3.3).
- (ii) Elements in Zone CF which intersect with the surface crack will be identified, extracted and form a sub-zone A for further processing (Figure 5.9). Note that more than one such sub-zone could be formed if it is required to include more than one surface crack in the model. The number and the location of elements extracted from Zone CF will depend on the length and the location of the crack at the welded joint.
- (iii) The crack element group which will be the sub-zone A is generated separately. A tube of “spider web” elements will be generated around the crack front firstly. In

order to simplify the connection of the “tube” and the adjacent elements, eight-element model is used as shown in Figure 5.10(a). Yang (1996) analyzed several general cases of cracked components and pointed out that at least three rings of elements should be used so that J-integral values can be calculated from sufficient contours to check the path independence of the results. Hence, four rings are used as shown in Figure 5.10(a). In order to capture the stress singularity near the crack front, elastic singular quarter-point prism elements are used in the first layer of the tube (Lie et al, 2005) and they are enclosed by three layers of 3-D 20-node quadratic brick elements (Figure 5.10(a)). The spider web tube will first be generated as a simple straight tube (Figure 5.10(b)). In order to match the 3D curved crack front defined in the geometrical model, a local orthogonal coordinate system $u'-v'-w'$ is then defined along the crack front (Figures 5.5(b) and 5.11(a)). The direction of the local orthogonal coordinate is shown in Figure 5.11(b). The vector w' is tangential to the crack front curvature and the vector u' is tangential to the crack surface. The directional cosines also satisfy the following equations:

$$l_i^2 + m_i^2 + n_i^2 = 1 \quad (5.17)$$

Hence, the direction of v' can be defined. Final mesh around the crack front will then be obtained by mapping or “twisting” the straight tube according to variation of the $u'-v'-w'$ system along the crack front (Figure 5.5 (b)) according to the relationship between the local $u'-v'-w'$ coordinate system and the global coordinate system $x-y-z$.

$$\begin{bmatrix} x \\ y \\ z \end{bmatrix} = \begin{bmatrix} l_1 & m_1 & n_1 \\ l_2 & m_2 & n_2 \\ l_3 & m_3 & n_3 \end{bmatrix} \begin{bmatrix} u' \\ v' \\ w' \end{bmatrix} \quad (5.18)$$

where l_i , m_i , and n_i are the direction cosines for the u' , v' , and w' directions.

- (iv) After the “twisted” tube is generated, it will then be inserted back to the extracted sub-zone A. In order to connect the tube and the outer regions well, transition regions are introduced as shown in Figure 5.12(a). In this region, another three element types are used, i.e. prism, tetrahedron and pyramid element. Prism elements are used as the transition elements between the crack front and the field far away from the crack; tetrahedron elements are adopted to link the crack elements and adjacent elements; and pyramid elements are used to fill the void arising when different elements are linked together in the field of the crack. The elements application in sub-zone A is shown in Figure 5.12 (b). Thus, the sub-zone A is generated as shown in Figure 5.13.
- (v) In order to insert the sub-zone A and connect it well with adjacent elements in Zone CF without distorting the shape of the elements too much, four blocks, named BLOCKA, BLOCKB, BLOCKC and BLOCKD, which are connected to the two crack tips are generated as shown in Figure 5.14.
- (vi) Finally, the whole mesh will be obtained with the processed zone CF is inserted back to the welded zone (Figure 5.15).

5.4 CONTACT MODELLING ON CRACK SURFACE

When some commercial programs such as ABAQUS (2004a) are used to analyze the FE models of joints, contact problems on the crack surfaces should be considered because the crack in the intersection of the SSHS joint may be closed due to some loading conditions. If the restriction between the crack surfaces is not given in this situation, one crack face will cross another by compressive stresses and the analysis results will be wrong. Chong Rhee et al (1991) had reported this problem in their study. There are two methods for modelling contact problems in ABAQUS (2004a), using contact surfaces or using contact elements. Burdekin et al (1998) used the method of contact elements in their study. INTERFACE elements called INTER3 were arranged between the crack surfaces in their FE models of joints, but they found that these contact elements influence the convergence of the results significantly. Chiew et al

(2001) adopted surface-based contact modelling in their study and found that this method has little influence on the convergence of the results. Hence, in this study a surface-based contact modelling method is used in the models. The advantage of this method is that it does not need to make any modifications on the original geometrical model. And its influence to the convergence of the SIF results will also be discussed later.

The contact surfaces are defined by the faces of the elements, which are on the crack faces, except those that have nodes on the crack front. Otherwise, it will cause problems when one contact surface is defined as a master surface and another as a slave surface. The reason is that the faces of these elements which have nodes on the crack front, have the same nodes as those on another crack faces. A program was specifically written for this purpose. It can automatically search the elements and their faces that are on the crack surfaces. These elements are then converted to the relative element types for contact purpose. For example, the C3D10 elements are converted to C3D10M elements. In the model, the contact surface in the inner crack face will be defined as a master surface and that in the outer crack face is a slave surface as illustrated in Figure 5.15.

5.5 CONVERGENCE STUDY

According to the theory of finite element method, the accuracy of the results increases as more elements are used in the model. When the density of the elements reaches to a certain value, the numerical results will converge correspondingly. Therefore, prior to any comprehensive investigation using the finite element model, it must be taken care that the model is behaving as would be anticipated. The mesh density must be sufficient to accurately conduct finite element analysis. On the other hand, a too fine mesh will waste computational effort. Hence, a convergence study is implemented to ensure that the mesh density of the model generated by the method proposed above is sufficiently fine.

5.5.1 Convergence Study of the Model without Cracks

In the study of HSS of a SSHS joint without cracks, only the stress distribution on the chord and brace surface is considered. Hence, only the segment number around the brace-chord intersection is changed. Due to the geometrical property of the SSHS joints, the high stress concentration usually appears at the corner. The mesh quality at the corner should have effect on the HSS results. In order to study the convergence of the model, three models are considered. One is the original model; two is the model with doubled mesh at the corner; and in the third one, the element number is doubled. The geometrical parameters of the models are same as Specimen II that used in the fatigue tests (Figure 3.3). The other details of the models are listed as follows:

$$b_0 = 350.0\text{mm} \quad t_0 = 16.0\text{mm}$$

$$b_1 = 200.0\text{mm} \quad t_1 = 16.0\text{mm}$$

$$L_{Ch} = 4130.0\text{mm} \quad L_{Br} = 2165.2\text{mm}$$

$$\text{Young's Modulus, } E = 210 \text{ kN/mm}^2$$

$$\text{Poisson ratio, } \nu = 0.3$$

Loading condition: 6 MPa tensile stress is applied at the end face of the brace.

Boundary condition: Both ends of the chord are fixed. The end of the brace is free.

Model 1: 6 elements along M3-CP3, 4 elements at the corner CP3, and 6 elements along CP3-M4 (please refer Figure 5.2);

Model 2: 6 elements along M3-CP3, 8 elements at the corner CP3, and 6 elements along CP3-M4 (please refer Figure 5.2);

Model 3: 12 elements along M3-CP3, 8 elements at the corner CP3, and 12 elements along CP3-M4 (please refer Figure 5.2)

The three models with different mesh density are generated and then analyzed using ABAQUS (2004a). The HSS distributions around the intersection are obtained. The results are compared in Figure 5.17. It can be seen that they are in complete agreement.

The results show that four elements at the corner are enough for the HSS analysis. The original mesh is good enough to conduct HSS analysis.

5.5.2 Convergence Study of the Model with Cracks

When a hollow section joint model is generated, SIF is introduced. For the convergence study of the model with crack, three models with cracks are generated, and analyzed using ABAQUS (2004a). The SIF results are compared. The details of the models are exactly the same as the models used in above convergence study of the model without cracks. The crack profiles are defined by the parameters s'_i , $i=1,2$ and l_d as discussed in Section 2.1.3 (Figure 5.5). The details of the models are listed as follows,

$$b_0 = 350.0\text{mm} \quad t_0 = 16.0\text{mm};$$

$$b_1 = 200.0\text{mm} \quad t_1 = 16.0\text{mm};$$

$$L_{Ch} = 4130.0\text{mm} \quad L_{Br} = 2165.2\text{mm};$$

$$\text{Crack profiles: } a' = 8.75 \text{ mm}, s'_1 = 45.6\text{mm}, s'_2 = 94.4\text{mm}, l = 195.0\text{mm} \text{ and } l_d = 140\text{mm};$$

The crack is located at corner CP4.

Young's Modulus, $E = 210 \text{ kN/mm}^2$

Poisson ratio, $\nu = 0.3$

Loading condition: 6 MPa tensile stress is applied at the end face of the brace.

Boundary condition: Both ends of the chord are fixed. The end of the brace is free.

Model 1: 6 elements along M3-CP3, 4 elements at the corner CP3, and 6 elements along CP3-M4 (please refer Figure 5.2);

Model 2: 6 elements along M3-CP3, 8 elements at the corner CP3, and 6 elements along CP3-M4 (please refer Figure 5.2);

Model 3: 12 elements along M3-CP3, 8 elements at the corner CP3, and 12 elements along CP3-M4 (please refer Figure 5.2).

The SIF results from the three models are compared in Figure 5.18. From the figure, it can be seen that Model 1 gives slightly small SIF value and the results from Model 2 and Model 3 are almost same. Hence, the density of Model 2 is enough for the further SIF analysis.

5.6 VALIDITY OF THE MODEL WITH AND WITHOUT CRACKS

One effective way to validate the modelling technique is to use this technique to generate the model of the actual specimens and compare the numerical results with these from the experiments. In Chapter 3 and 4, four full-scale specimens were tested and both static and fatigue performance were studied. Here the four test results are used to validate the numerical model with and without cracks. The geometry, the material properties and the loading conditions of the numerical models are exactly the same as those of the specimens tested in Chapter 3 and 4. The applied loads are obtained from the readings of strain gauge on the brace directly. The boundary conditions are that the two ends of the chord are fixed rigidly, and the end of the brace is free.

5.6.1 SCF Comparison with Test Results

Each of the specimens is modeled using the modelling technique without crack and the models are then analyzed using ABAQUS (2004a). The circumference position of the interesting point for each specimen, which is also the strain gauge location, is shown in Chapter 3 (please refer to Figure 3.5). The hot spot stress (HSS) is calculated using the quadratic interpolation method as do in the experimental analysis. The SCF distribution around the connection of the four specimens under axial load (AX), in-plane-bending (IPB) and out-of-plane-bending (OPB) is shown in Figures 5.19 to 5.22. The trend of the SCF distribution is same. In Figure 5.19, the values agree well except for point 4 on chord under AX, which may be caused by strain gauge problem. Similar trends for SCF distribution and better agreement on SCF value can be seen under IPB and OPB. The

difference may be caused by the following two reasons: (1) The strain gauge location. As the strain gauge putting is a difficult job, slight inaccuracies in sitting strain gauge will cause the difference from the FEM results; (2) the weld surface is not as smooth as the numerical one. In FEM, the weld toe could be a smooth curve. However, in practice, the weld toe is zigzag. In numerical modelling, mean values of the weld width (w) and height (h) (as shown in Figure 3.4 in the thesis) are used. However, in practice the weld width and weld height of the four sides of one specimen are different. The comparison of the SCF distribution around the connection of Specimen II, III and IV are conducted in Figures 5.20 to 5.22. Same trend and good agreement can be obtained. From these figures, it can be seen that the FEM give a slight higher SCF value on braces. Arising from the comparisons, the numerical model without cracks can be used for the further large number of SCF and HSS analysis.

5.6.2 SIF Comparison with Test Results

5.6.2.1 Stress intensity factor from numerical analysis

After the model with crack is generated, stress intensity factor is introduced to validate the model. Using the ABAQUS (2004a) finite element program, the model is analyzed and the SIF of the crack can be obtained. There are several well-established procedures for this purpose as discussed in Chapter 2, for example, the commonly used J-integral method and the method of displacement extrapolation used by Chong Rhee et al (1991) and Bowness and Lee (1995). Currently, another method called the interaction integral method provided by ABAQUS (2004) is used to extract the individual SIFs directly for a crack under the mixed mode loading conditions. This method is based on the work by Shih and Asaro (1988), and it has the advantage of being rather insensitive to mesh refinement and can be applied easily under the mixed mode situations. Detailed derivation of the equations for this method can be found in the ABAQUS (2001) manuals and Shih and Asaro (1988). Lie et al (2004) have compared SIF values from displacement extrapolation method and the interaction integral method. They found that SIF values from the interaction integral method are almost between the results

evaluated by displacement extrapolation method with plain strain assumption and those with plain stress assumption. They are very close to the results evaluated by displacement extrapolation method with plain strain assumption except for the points of crack tip on chord surface.

For the sake of comparison, the K_I , K_{II} and K_{III} from numerical analyses are combined together and represented by an equivalent SIF called K_e , which is assumed as a crack driving force parameter for a mixed mode fracture problem (Chong Rhee et al, 1991). The relationship between K_e and K_I , K_{II} , K_{III} is

$$K_e = [K_I^2 + K_{II}^2 + K_{III}^2 / (1 - \nu)]^{1/2} \quad (5.19)$$

where ν is Poisson's ratio.

5.6.2.2 Stress intensity factor from experiments

The validity of the FE model with crack is verified by comparing the SIF results from the model produced by the proposed mesh generation technique with these from the large-scale testing conducted in Chapter 4. The experimental results can be converted to SIFs by Paris' law,

$$da / dN = C(\Delta K)^m \quad (5.20)$$

where da/dN is crack growth rate which can be obtained from the experimental results, ΔK is the range of SIFs, C and m are the material constants, and $C = 8.02 \times 10^{-12} (\text{m/cycle})(\text{MPa} \cdot \text{m}^{1/2})^{-2.92}$ and $m = 2.92$ (Vosikovsky et al, 1985) are used in this study.

5.6.2.3 Crack surface measured from experiments

The crack surface can be defined by the parameters, $\theta_s^i(0)$, $\theta_s^{\max}(0)$, $\theta_s^i(z'_c)$, $\theta_s^{\max}(z'_c)$, as analyzed in Section 2.1.2 (please refer to Figure 5.4). Hence, the equation of the crack surface can be obtained by introducing the measured data, $\theta_s^i(0)$, $\theta_s^{\max}(0)$, $\theta_s^i(z'_c)$, and $\theta_s^{\max}(z'_c)$ from the fatigue tests into Eqns. (5.12), (5.13) and (5.14). The values of the measured data, $\theta_s^i(0)$, $\theta_s^{\max}(0)$, $\theta_s^i(z'_c)$, and $\theta_s^{\max}(z'_c)$ from the fatigue tests are listed in Table 5.2 (please also refer to Figures 4.38 to 4.41). The equations of the crack surfaces for the four specimens are as follows:

For Specimen I,

For $0 \leq z' \leq z'_c$

$$\begin{cases} x' = z' \tan\left(\frac{12z' + 4t_0}{3t_0(16L_w + \pi r_w)} \pi s' + \frac{1}{12} \pi\right) & \text{for } 0 \leq s' \leq L_w \\ x' = z' \tan\left(-\frac{48z' + 16t_0}{3t_0 r_w(16L_w + \pi r_w)} (s' - L_w - \frac{\pi r_w}{8})^2 + (\frac{z'}{4t_0} + \frac{1}{6}) \pi\right) & \text{for } L_w \leq s' \leq L_w + \pi r_w / 2 \end{cases} \quad (5.21a)$$

For $z'_c \leq z' \leq t_0$,

$$x' = \left[\tan(\theta_s(z'_c, s')) + \frac{(\theta_s(z'_c, s') - \theta_s(0, s'))}{\cos^2(\theta_s(z'_c, s'))} \right] (z' - 0.8t_0) + 0.8t_0 \tan(\theta_s(z'_c, s')) \quad (5.21b)$$

where,

$$\begin{cases} \theta_s(0, s') = \frac{4}{3(16L_w + \pi r_w)} \pi s' + \frac{1}{12} \pi \\ \theta_s(z'_c, s') = \frac{68}{15(16L_w + \pi r_w)} \pi s' + \frac{1}{12} \pi \end{cases} \quad \text{for } 0 \leq s' \leq L_w$$

$$\begin{cases} \theta_s(0, s') = -\frac{16}{3r_w(16L_w + \pi r_w)} (s' - L_w - \frac{\pi r_w}{8})^2 + \frac{1}{6} \pi \\ \theta_s(z'_c, s') = -\frac{208}{15r_w(16L_w + \pi r_w)} (s' - L_w - \frac{\pi r_w}{8})^2 + \frac{11}{30} \pi \end{cases} \quad \text{for } L_w \leq s' \leq L_w + \pi r_w / 2$$

For Specimen II,

For $0 \leq z' \leq z'_c$

$$\begin{cases} x' = z' \tan\left(\frac{20z' + 8t_0}{9t_0(16L_w + \pi_w)} \pi s' + \frac{1}{12} \pi\right) & \text{for } 0 \leq s' \leq L_w \\ x' = z' \tan\left(-\frac{80z' + 32t_0}{9t_0 r_w(16L_w + \pi_w)} \left(s' - L_w - \frac{\pi_w}{8}\right)^2 + \left(\frac{z'}{t_0} + 1\right) \frac{5\pi}{36}\right) & \text{for } L_w \leq s' \leq L_w + \pi_w/2 \end{cases} \quad (5.22a)$$

For $z'_c \leq z' \leq t_0$,

$$x' = \left[\tan(\theta_s(z'_c, s')) + \frac{(\theta_s(z'_c, s') - \theta_s(0, s'))}{\cos^2(\theta_s(z'_c, s'))} \right] (z' - 0.8t_0) + 0.8t_0 \tan(\theta_s(z'_c, s')) \quad (5.22b)$$

where,

$$\begin{cases} \theta_s(0, s') = \frac{8}{9(16L_w + \pi_w)} \pi s' + \frac{1}{12} \pi \\ \theta_s(z'_c, s') = \frac{8}{3(16L_w + \pi_w)} \pi s' + \frac{1}{12} \pi \end{cases} \quad \text{for } 0 \leq s' \leq L_w$$

$$\begin{cases} \theta_s(0, s') = -\frac{32}{9r_w(16L_w + \pi_w)} \left(s' - L_w - \frac{\pi_w}{8}\right)^2 + \frac{5}{36} \pi \\ \theta_s(z'_c, s') = -\frac{32}{3r_w(16L_w + \pi_w)} \left(s' - L_w - \frac{\pi_w}{8}\right)^2 + \frac{1}{4} \pi \end{cases} \quad \text{for } L_w \leq s' \leq L_w + \pi_w/2$$

For Specimen III,

For $0 \leq z' \leq z'_c$

$$\begin{cases} x' = z' \tan\left(\frac{430z' + 104t_0}{90t_0(16L_w + \pi_w)} \pi s' + \frac{1}{12} \pi\right) & \text{for } 0 \leq s' \leq L_w \\ x' = z' \tan\left(-\frac{860z' + 208t_0}{45t_0 r_w(16L_w + \pi_w)} \left(s' - L_w - \frac{\pi_w}{8}\right)^2 + \left(\frac{43z'}{144t_0} + \frac{5}{36}\right) \pi\right) & \text{for } L_w \leq s' \leq L_w + \pi_w/2 \end{cases} \quad (5.23a)$$

For $z'_c \leq z' \leq t_0$,

$$x' = \left[\tan(\theta_s(z'_c, s')) + \frac{(\theta_s(z'_c, s') - \theta_s(0, s'))}{\cos^2(\theta_s(z'_c, s'))} \right] (z' - 0.8t_0) + 0.8t_0 \tan(\theta_s(z'_c, s')) \quad (5.23b)$$

where,

$$\begin{cases} \theta_s(0, s') = \frac{52}{45(16L_w + \pi r_w)} \pi s' + \frac{1}{12} \pi \\ \theta_s(z'_c, s') = \frac{224}{45(16L_w + \pi r_w)} \pi s' + \frac{1}{12} \pi \end{cases} \quad \text{for } 0 \leq s' \leq L_w$$

$$\begin{cases} \theta_s(0, s') = -\frac{208}{45r_w(16L_w + \pi r_w)} (s' - L_w - \frac{\pi r_w}{8})^2 + \frac{5}{36} \pi \\ \theta_s(z'_c, s') = -\frac{896}{45r_w(16L_w + \pi r_w)} (s' - L_w - \frac{\pi r_w}{8})^2 + \frac{17}{45} \pi \end{cases} \quad \text{for } L_w \leq s' \leq L_w + \pi r_w / 2$$

For Specimen IV,

For $0 \leq z' \leq z'_c$

$$\begin{cases} x' = z' \tan\left(\frac{175z' + 48t_0}{45t_0(16L_w + \pi r_w)} \pi s' + \left(\frac{5z'}{72t_0} + \frac{2}{45}\right)\pi\right) \\ x' = z' \tan\left(-\frac{700z' + 192t_0}{45t_0r_w(16L_w + \pi r_w)} (s' - L_w - \frac{\pi r_w}{8})^2 + \left(\frac{5z'}{16t_0} + \frac{1}{9}\right)\pi\right) \end{cases} \quad \begin{matrix} \text{for } 0 \leq s' \leq L_w \\ \text{for } L_w \leq s' \leq L_w + \pi r_w / 2 \end{matrix}$$

(5.24a)

For $z'_c \leq z' \leq t_0$,

$$x' = \left[\tan(\theta_s(z'_c, s')) + \frac{(\theta_s(z'_c, s') - \theta_s(0, s'))}{\cos^2(\theta_s(z'_c, s'))} \right] (z' - 0.8t_0) + 0.8t_0 \tan(\theta_s(z'_c, s')) \quad (5.24b)$$

where,

$$\begin{cases} \theta_s(0, s') = \frac{16}{15(16L_w + \pi r_w)} \pi s' + \frac{2}{45} \pi \\ \theta_s(z'_c, s') = \frac{188}{45(16L_w + \pi r_w)} \pi s' + \frac{1}{10} \pi \end{cases} \quad \text{for } 0 \leq s' \leq L_w$$

$$\begin{cases} \theta_s(0, s') = -\frac{192}{45r_w(16L_w + \pi r_w)} (s' - L_w - \frac{\pi r_w}{8})^2 + \frac{1}{9} \pi \\ \theta_s(z'_c, s') = -\frac{752}{45r_w(16L_w + \pi r_w)} (s' - L_w - \frac{\pi r_w}{8})^2 + \frac{13}{36} \pi \end{cases} \quad \text{for } L_w \leq s' \leq L_w + \pi r_w / 2$$

5.6.2.4 SIF comparison

The specimens are modeled with the actual crack profiles. The crack profiles of the models are all based on the information obtained from the fatigue tests as listed in Table 5.3. The crack surface is defined by the Eqns. (5.21) to (5.24). Numerical analysis is conducted by ABAQUS (2004). It is noted that these SIF results are the average values of the results from the different contours except that from the first contour abutting the crack tip because numerical tests have shown that this contour cannot provide highly accurate results (ABAQUS 2004).

The numerical SIF results calculated from FE model with the measured crack surfaces of Specimen I are compared with the experimental results at the crack deepest point in Figure 5.23. From the figure, we can see that both trend and values of the numerical SIFs match well with the experimental results. The difference of the SIF results from experiment and numerical analyses is less than 5%.

Figure 5.24 shows the SIF comparison of Specimen II. As mentioned in Chapter 4, Specimen II is the case with two cracks at two adjacent corners. During the testing of Specimen II, no attention was paid to other corners because no cracks were expected at other three corners. Hence, there is no idea about the second crack's initiation and propagation. Due to the lack of the second crack information, only one crack is involved in the numerical analysis for the comparison. The crack profiles are as same as these of the main crack recorded by ACPD technique during testing. And during the test, eccentricity is introduced due to the dropdown of the brace end. In the numerical analysis, the eccentricity is eliminated and actual load is applied. From the figure, it can be seen that the trend agrees well. Before a'/t_0 is less than 0.4, the FEM results are slight higher than the experimental results. After that, the SIF values from FEM and test match well.

The SIF comparison between numerical analysis and experimental study of Specimen III is shown in Figure 5.25. During testing, one crack was detected, but the crack was found to deviate from weld toe at one crack tip. In the numerical modelling, actual

crack length recorded by ACPD was used while the crack was along the weld toe. From the figure, it can be seen that both the trend and the SIF values match well. Only at a'/t_0 is at 0.7-1.0, at the drops of the experimental SIF, there is a slight discrepancy. This may be caused by the difference between the model and the actual crack which is branching out.

The SIF comparison of Specimen IV is shown in Figure 5.26. This is the case that one crack appears with the crack tip branching out. Same way is used in the numerical modelling with Specimen III. Actual crack length is used while the crack is along the weld toe. The figure shows that the trends of the SIF results agree well. The numerical result has slightly higher value. The maximum difference between the FEM and the numerical results is less than 26%.

From above comparisons and analysis, the validity of the model is proven. The proposed mesh generation technique in this paper is stable and reliable for the further fracture mechanics analysis of square-to-square tubular T-joints with or without cracks.

5.7 CONCLUDING REMARKS

In this chapter, a technique to generate consistent and accurate geometrical finite element models for general cracked SSHS T-joints is proposed. The models can include a surface crack of arbitrary length and location along the joint. The crack surface can be presented by a function which includes two variables; one is used to define the crack depth direction (z'), and the other is used to define the location of the crack along the weld toe (s'). The crack shape can be described either by a unsymmetrical bi-elliptical curve. This modelling technique was then applied to analyze four hollow section T-joint specimens which have been tested to failure earlier. The SCFs and SIFs obtained numerically compared favorably with those obtained from experiments. Thus the validity of the technique is demonstrated and the comparison

Chapter 5 Geometrical modelling and FEM mesh generation for SSHS T-joints

shows that the proposed modelling technique is stable and reliable for the further analysis of the SSHS T-joints with or without cracks.

Figure 5.1 Configuration of the joint and global coordinate system (x - y - z)

Figure 5.2 Modelling of weld toe

(a) Plan view

(b) Section $I'-I'$ ($y'=0$)

(c) Surface model used

Figure 5.3 Geometrical model for the crack surface

Figure 5.4 Assumed variation of $\theta_s(z')$ with s'

(a)

(b)

Figure 5.5 Modeling of crack front

Figure 5.6 Mesh Generation of the weld joint without surface crack

Figure 5.7 Full model without crack

(a) Model with coarse mesh

(b) Model with fine mesh

Figure 5.8 Model with different density mesh

(a) Zone CF with space for one crack group

(b) Zone CF with space for two crack groups

Figure 5.9. Elements in zone CF are extracted to form sub-zone A

Figure 5.10 Generation of mesh around the crack front

Figure 5.11 The direction of u' , v' and w'

Figure 5.12 Mesh in crack element group sub-zone A

Figure 5.13 Element group sub-zone A

Figure 5.14 Crack group with BLOCKA, BLOCKB, BLOCKC and BLOCKD

Figure 5.15 Full model with cracks

Figure 5.16 Definition of master surface and slave surface

Figure 5.17 HSS comparisons from the model with different mesh density

Figure 5.18 SIF comparisons from the model with different mesh density

(a) Comparison of SCF distribution on chord

(b) Comparison of SCF distribution on brace

Figure 5.19 Comparison of SCF distribution of Specimen I under AX, IPB and OPB

(a) Comparison of SCF distribution on chord

(b) Comparison of SCF distribution on brace

Figure 5.20 Comparison of SCF distribution of Specimen II under AX, IPB and OPB

(a) Comparison of SCF distribution on chord

(b) Comparison of SCF distribution on brace

Figure 5.21 Comparison of SCF distribution of Specimen III under AX, IPB and OPB

(a) Comparison of SCF distribution on chord

(b) Comparison of SCF distribution on brace

Figure 5.22 Comparison of SCF distribution of Specimen IV under AX, IPB and OPB

Figure 5.23 SIF comparisons between test and FEM of Specimen I

Figure 5.24 SIF comparisons between test and FEM of Specimen II

Figure 5.25 SIF comparisons between test and FEM of Specimen III

Figure 5.26 SIF comparisons between test and FEM of Specimen IV

Chapter 5 Geometrical modeling and FEM mesh generation for SSHS T-joints

Table 5.1 Element types used in the mesh generation of SSHS T-joints

	Element Types	No. of Nodes	
1.	Hexahedral/Cubic Element – (H20)	20
2.	Prism/Wedge – (P15)	15
3.	Quarter Point/Crack Element – (QP15) (Collapsed Prism)	15
4.	Tetrahedron – (T10)	10
5.	Pyramid – (PR20) (Collapsed Hexahedral)	20

Table 5.2 The values of measured crack surface data from fatigue tests

Specimen	$\theta_s^i(0)$	$\theta_s^{\max}(0)$	$\theta_s^i(z'_c)$	$\theta_s^{\max}(z'_c)$
I	$\frac{1}{12}\pi$	$\frac{1}{6}\pi$	$\frac{1}{12}\pi$	$\frac{11}{30}\pi$
II	$\frac{1}{12}\pi$	$\frac{5}{36}\pi$	$\frac{1}{12}\pi$	$\frac{1}{4}\pi$
III	$\frac{1}{15}\pi$	$\frac{5}{36}\pi$	$\frac{1}{15}\pi$	$\frac{17}{45}\pi$
IV	$\frac{2}{45}\pi$	$\frac{1}{9}\pi$	$\frac{1}{10}\pi$	$\frac{13}{36}\pi$

Chapter 5 Geometrical modeling and FEM mesh generation for SSHS T-joints

Table 5.3 Crack profiles used in finite element models

Crack profile	a' (mm)	s'_1 (mm)	s'_2 (mm)	l (mm)	l_d (mm)
	Specimen I				
1	2.10	102.50	145.50	43	18
2	2.93	82.50	150.50	68	38
3	4.46	80.50	150.50	70	40
4	6.26	77.50	157.50	80	43
5	8.08	65.50	165.50	100	55
6	9.95	63.50	167.50	104	57
7	11.34	60.50	170.50	110	60
8	13.27	60.50	170.50	110	60
9	15.09	60.50	170.50	110	60
10	17.00	50.50	172.50	122	70
11	19.33	35.50	175.50	140	85
Load(KN)	AX(150)+IPB(5.82)+OPB(-3.97)				
	Specimen II				
1	1.75	44.41	114.41	70	50
2	2.35	44.41	114.41	73	50
3	3.20	42.41	117.41	75	52
4	4.96	19.41	144.41	125	85
5	6.05	19.41	144.41	125	85
6	7.74	14.41	144.41	130	85
7	8.75	14.41	144.41	130	85
8	11.34	0.59	144.41	145	95
9	13.93	5.59	144.41	150	100
10	15.33	25.59	149.41	175	120
11	17.85	45.59	149.41	195	140
12	19.38	65.59	154.41	220	160
Load(KN)	AX(75)+IPB(-3.07)+OPB(-0.413)				

Chapter 5 Geometrical modeling and FEM mesh generation for SSHS T-joints

Crack profile	a' (mm)	s'_1 (mm)	s'_2 (mm)	l (mm)	l_d (mm)
	Specimen III				
1	1.42	73.36	133.36	60	25
2	2.59	69.36	136.36	67	29
3	3.99	68.36	136.36	68	30
4	5.95	63.36	140.36	77	35
5	8.41	58.36	144.36	86	40
6	10.78	51.36	148.36	97	47
7	13.03	40.36	151.36	111	58
8	15.79	31.36	158.36	127	67
9	18.43	18.36	158.36	140	80
10	20.03	8.36	158.36	150	90
Load(KN)	IPB(-4.17)+OPB(-3.8)				
	Specimen IV				
1	1.27	77.49	120.49	43	20
2	3.13	57.49	127.49	70	40
3	5.09	42.49	137.49	95	55
4	6.56	34.49	137.49	103	63
5	8.18	27.49	137.49	110	70
6	9.81	22.49	137.49	115	75
7	11.30	7.49	137.49	130	90
8	12.81	2.49	137.49	135	95
9	14.88	27.51	137.49	165	125
10	16.43	37.51	137.49	175	135
Load(KN)	AX(70)+IPB(1.4)+OPB(-1.693)				
Note: Positive loads are tension loads, and negative loads are compression loads					

CHAPTER 6

FINITE ELEMENT ANALYSIS ON CRACK PROFILES, WELD SIZE AND CONTACT PROBLEMS IN SSHS T- JOINTS

6.1 INTRODUCTION

Stress concentration factor (SCF) and stress intensity factor (SIF) play key roles in assessing the fatigue life of hollow section joints. The definitions of SCF and SIF have been given in Chapter 2. Generally, SCF is obtained from static state for a joint without any initial defects and then the hot spot stress (HSS) can be calculated from SCF. The whole fatigue life which includes both crack initiation phase and crack propagation phase can be assessed by referring to the S-N curve (Zhao et al, 2000). However, when there already exists a crack-like defect in the hollow section joint, the above procedure to assess the fatigue life of the joints is no longer applicable. SIF, which is used to measure the strength of the local crack-tip fields, must be introduced. Fracture mechanics approach can be used to assess the residual life of the hollow section joints with one crack by accurate estimation of the SIF. However, the determination of SIF in hollow section joints is a challenging task until now. The SIF is a function of the applied stress field, crack length and geometry and is used in conjunction with crack growth such as the Paris equation.

In Chapter 3 and Chapter 4, large-scale SSHS T-joints are tested. The fatigue test results provide valuable information about the fatigue performance of the SSHS T-joints. However, it is unrealistic to carry out a great number of full-size tests since the

fatigue testing is extremely time-consuming and expensive. Furthermore, it is very difficult to accurately conduct a large-scale test in the controlled laboratory due to the complicated load cases and boundary conditions. In Chapter 5, a systemic procedure of a finite element modelling is introduced. It has been demonstrated to be stable and reliable for further analysis of the fatigue behaviour of the SSHS T-joints. It provides an alternative and inexpensive method to study and comprehend the fatigue performance of the SSHS T-joints.

Hence, in this chapter, the finite element modelling proposed in Chapter 5 is used to conduct the analysis of the fatigue performance of the SSHS T-joints. The objective of this chapter is to understand the influence of various factors on the SIF calculation and try to understand the fatigue behaviour of the SSHS T-joints. Later on, large amount of assessment of the fatigue life of the SSHS T-joints can be carried on even without crack geometry provided by large-scale fatigue tests. In this chapter, finite element analysis (FEA) is conducted on the crack profiles including crack surface, crack shape, two crack problems, contact problems and the influence of the weld size on the SIF in SSHS T-joints.

6.2 FE ANALYSIS ON CRACK PROFILES

Large-scale fatigue tests in Chapter 3 and 4 show that the crack profiles in SSHS T-joints are quite different from those in circle hollow section (CHS) joints. Hence, the previous conclusions drawn from the CHS joints cannot be used directly, such as crack surface and crack shape. Besides this, new phenomena have been observed in SSHS joints, i.e. crack branching and two crack problems. Hence, investigations must be carried out about the influence of the crack profiles on the SIF calculation.

6.2.1 Crack Surface

Many researchers have been involved in the modelling of the CHS joints with cracks (Rhee et al, 1991, Cao et al, 1997, Lee et al, 1999, and Lie et al, 2000) but most of them simplified the crack surface with a straight line perpendicular to the chord surface (Rhee et al, 1991 and Cao et al, 1997) or a straight line going through the intersection point of the chord and brace's midlines (Lee et al, 1999 and Lie et al, 2000). This is because of the extreme difficulty in modelling the actual situation involving a crack curving around the hollow section joint intersection as well as under the weld toe. Bowness and Lee (1998) studied the fatigue crack surface under the weld toe in the tubular joint and found that the stress intensity factors derived from a straight perpendicular crack is not significantly different from that for a crack with the correct crack surface. It means that the crack surface under the weld toe can be simplified to a straight perpendicular line in CHS joints. However, these experiences cannot be used in the SSHS joints directly due to the geometric difference. Fatigue tests on SSHS T-joints in Chapter 3 and 4 showed that the crack surface was twisted since it not only curved under weld toe but also changed with the crack location at the intersection. Hence, FEA on crack surface in SSHS T-joints is conducted. The objective of the analysis is to quantify the crack surface effect on the SIF. This is achieved by analysing the four full-scale specimens tested in Chapter 3 and 4 with cracks of various surfaces using the modelling technique proposed in Chapter 5 and then correlating the results with the experimental observations. Based on the comparisons and experimental observations, a general crack surface is proposed.

The dimensions of the models are the same as the specimens in Chapter 3 and 4 (please refer to Figure 3.2). The load is the same as the actual fatigue load applied to the specimens (please refer to Table 5.1). The crack location and crack shape are all from the measurement of the four tests (please refer to Table 5.1). Straight line crack surfaces, inclined at five different angles, i.e. $\theta_s(z') = 0, \pi/12, \pi/6, \pi/4$ and $\pi/3$ are studied as shown in Figure 6.1 and the crack surface does not change with the crack

location around the intersection. That means the crack surface just curves around weld toe and it is not a twisted surface. The local coordinates are set as shown in Figure 6.1 which is matched with the numerical modelling in Chapter 5 (please refer to Figure 5.3). The equations of the crack surface can be represented as follows,

$$x' = z' \tan(\theta_s(z')) \quad (6.1)$$

The finite element modelling technique developed in Chapter 5 is used to generate the models with different crack surfaces and the models are then analyzed in ABAQUS (2004a). The SIF values at the deepest point for different crack surface are presented in Figures 6.2 to 6.5. Furthermore, the SIF at the deepest point from the test results are also shown in these figures as benchmark. The value comparison is listed in Table 6.1.

From Figures 6.2 to 6.5 and Table 6.1, the SIF values at the deepest point diverge largely with the experimental results and the SIF values from these different crack surfaces are quite different. That means the crack surface has significant effect on the SIF values. In the figures, when the crack depth is quite small or quite big, especially when the crack depth ratio $a'/t_0 \geq 0.6$, the influence is notable. The models with surface inclined at small values of $\theta_s(z')$ (0 , $\pi/12$, and $\pi/6$) result in overestimation SIF values when a'/t_0 is small but show a severe underestimation of SIF values when $a'/t_0 \geq 0.6$. However, the SIF values in the models with crack surface inclined at $\theta_s(z') = \pi/4$ and $\theta_s(z') = \pi/3$ are much higher than the experimental results when $a'/t_0 \geq 0.6$. The divergence* could be 40% (in Specimen III). These comparisons show that the straight lines cannot represent the crack surface in SSHS T-joints. This also

$$* \text{ Divergence} = (SIF_{\theta_s(z')} - SIF_{Exp.}) / SIF_{Exp.} \times 100\%$$

$SIF_{\theta_s(z')}$ = SIF from the model with crack surface inclined at angle $\theta_s(z')$

$SIF_{Exp.}$ = SIF calculated from experimental results

indicates that the crack surface is one of the factors that determine the crack growth in SSHS joints and the models with constant $\theta_s(z')$ cannot lead to satisfactory prediction of SIFs. Hence, accurate FE modelling of the crack surface is necessary in the calculation of SIFs.

This conclusion is very different from that in CHS joints drawn by Bowness and Lee (1994), who carried out a numerical investigation about the crack surface using three-dimensional finite elements and pointed out that the crack growth in a circle tubular joint was governed purely by the stress intensity factor of opening mode rather than the mixed-modes which are the results of modelling a surface with no or incorrect crack.

Hence, in the finite element analysis of the SSHS T-joints, modelling of accurate crack surface is needed. However, it is impossible to provide the crack surface geometry for each FEA using the large-scale test results. So, a general crack surface equation which can be used for all the load cases and joint dimensions should be proposed. This equation may not be expected to give very accurate SIF values for all different cases since it is nearly impossible to be achieved, but at least it must be able to generate an acceptable SIF value for each case within certain error limits.

From the crack surface measurement after the full-scale fatigue tests finished, it is known that the crack surface can be modelled as a curve for $0 \leq z' \leq z'_c$ and a straight line for $z'_c \leq z' \leq t_0$ under the weld toe (please refer to Figure 5.3). The value z'_c is the critical depth for the crack. From the measurement data, $z'_c = 0.8t_0$ is adopted. The values of $\theta_s(z')$ will increase linearly with z' . Along the weld toe (crack length direction), piecewise function can be employed. The detailed derivation of the crack surface expression can be found in Chapter 5 (Section 5.1.2) where it is presented as Eqn. (5.14) in the local coordinates. From Eqns (5.12) and (5.13), it is known that the equation can be obtained once the values of $\theta_s^i(0)$, $\theta_s^{\max}(0)$, $\theta_s^i(z'_c)$ and $\theta_s^{\max}(z'_c)$ are known.

Based on the data measured in the four tests, the following values are adopted for $\theta_s^i(0)$, $\theta_s^{\max}(0)$, $\theta_s^i(z'_c)$ and $\theta_s^{\max}(z'_c)$,

$$\theta_s^i(0) = \theta_s^i(z'_c) = \pi/12$$

$$\theta_s^{\max}(0) = \pi/6$$

$$\theta_s^{\max}(z'_c) = \pi/3$$

Hence, the general equation that can be expressed as follows,

For $0 \leq z' \leq z'_c$

$$\begin{cases} x' = z' \tan\left(\frac{10z' + 4t_0}{3t_0(16L_w + \pi r_w)} \pi s' + \frac{1}{12} \pi\right) & \text{for } 0 \leq s' \leq L_w \\ x' = z' \tan\left(-\frac{40z' + 16t_0}{3t_0 r_w(16L_w + \pi r_w)} \left(s' - L_w - \frac{\pi r_w}{8}\right)^2 + \left(\frac{5z'}{36t_0} + \frac{5}{6}\right) \pi\right) & \text{for } L_w \leq s' \leq L_w + \frac{\pi r_w}{2} \end{cases} \quad (6.2a)$$

For $z'_c \leq z' \leq t_0$,

$$x' = \left[\tan(\theta_s(z'_c, s')) + \frac{(\theta_s(z'_c, s') - \theta_s(0, s'))}{\cos^2(\theta_s(z'_c, s'))} \right] (z' - 0.8t_0) + 0.8t_0 \tan(\theta_s(z'_c, s')) \quad (6.2b)$$

where,

$$\begin{cases} \theta_s(0, s') = \frac{4}{3(16L_w + \pi r_w)} \pi s' + \frac{1}{12} \pi \\ \theta_s(z'_c, s') = \frac{4}{(16L_w + \pi r_w)} \pi s' + \frac{1}{12} \pi \end{cases} \quad \text{for } 0 \leq s' \leq L_w$$

$$\begin{cases} \theta_s(0, s') = -\frac{16}{3r_w(16L_w + \pi r_w)} \left(s' - L_w - \frac{\pi r_w}{8}\right)^2 + \frac{5}{6} \pi \\ \theta_s(z'_c, s') = -\frac{16}{r_w(16L_w + \pi r_w)} \left(s' - L_w - \frac{\pi r_w}{8}\right)^2 + \frac{17}{18} \pi \end{cases} \quad \text{for } L_w \leq s' \leq L_w + \frac{\pi r_w}{2}$$

The parameters in Eqn. (6.2) are referred to Figure 5.3. In order to illustrate the validity of Eqn. (6.2), the four specimens tested in Chapter 3 and 4 are modelled with the crack surface defined by Eqn. (6.2). The crack location and crack length are all measured from fatigue tests (please refer to Table 5.1). The models are then analyzed in ABAQUS (2004a). The load conditions are as same as the fatigue load in tests (please refer to Table 5.1). The boundary conditions are all the same as the tests, i.e. both ends of the chord are fixed; the end of brace is free.

The SIF results at the deepest point are compared with these from the models with the measured crack surface defined by Eqns. (5.21)-(5.24) in Figures 6.6 to 6.9. SIF values from the test results are shown in these figures as benchmarks. It can be seen that for all the specimens, the models with proposed crack surface result in accurate and slightly conservative prediction of SIF for the whole range of a'/t_0 . The maximum relative error is about 15% when compared with experimental results. Furthermore, the models with proposed crack surface tend to over-predict the SIF values near end of the fatigue life of the joints (i.e. $a'/t_0 \geq 1.0$). Hence, the proposed equation for the crack surface can be used for all the crack cases in SSHS joints. However, further validation should be carried out.

The SIF distributions along the crack front are also investigated. The four specimens are modelled again and the crack profiles are selected from the Table 5.1. The selected crack profiles are listed in Table 6.2. Load and boundary conditions are all the same as the experimental one.

The SIF distributions along the crack front from the models with different crack surfaces (the proposed crack surface and measured crack surface) are compared in Figures 6.10 to 6.13. From the figures, it can be seen that both the FEA results have the highest SIF values at the deepest points except the two crack tips. That means it is reasonable to only investigate the SIF values at the deepest point when life assessment

is calculated. The figures also show that the SIF distribution from the models with proposed crack surface and measured one match well and the divergence is less than 5%. This further proves that the proposed crack surface equation (Eqn. (6.2)) can be used as a general representation for crack surface in SSHS T-joints.

6.2.2 Crack Shape

Another investigation made in this study is the effect of the crack shape in the modelling. The crack shape is often modelled as a semi-ellipse in CHS joints (Bowness and Lee, 1994, Rhee et al, 1991, and Chiew et al, 2001). The experimental investigation conducted by Huang (2002) also showed that the crack shape CHS T-joints is close to a semi-ellipse. Huang (2002) also conducted investigations on the effect of crack shape on the SIF values at the deepest point by modelling the crack shapes with polynomial curve, semi-ellipse, trapezoid curve, and triangle curve. He found that the results of stress intensity factor are sensitive to the crack shape modelled and the accurate crack shape modelling (polynomial curve) will make the results better at the deepest point of crack. Nevertheless in practice, using the semi-ellipse to model the crack in the CHS joints can be a good alternative method and one can also get relatively accurate results at the deepest point of crack.

All the crack shapes used in Huang's studies were symmetrical. The deepest point is located at the middle of the crack length. However, the experimental studies on SSHS T-joints in Chapter 4 clearly show that the crack shape is unsymmetrical especially when the crack is very deep (please refer to Figure 4.42). It could be very long in one direction. In order to ensure that the spatial derivative (slope) of the crack shape at the deepest point is zero, bi-elliptical curve is used as the representation of the crack shape in this study. As discussed in Chapter 5, the crack shape (front) can be defined once the parameters s'_1 , s'_2 , l_d and a' (please refer to Section 5.2.2) are known. The Eqn.(5.16) is repeated here,

$$\text{For } 0 \leq s' \leq s'_1 + l_d \text{ and } z \geq 0: \quad \frac{(s' - (s'_1 + l_d))^2}{(l_d)^2} + \frac{(z')^2}{(a')^2} = 1 \quad (5.16a)$$

$$\text{For } s'_1 + l_d < s' \leq s'_2 \text{ and } z \geq 0: \quad \frac{(s' - (s'_1 + l_d))^2}{(l - l_d)^2} + \frac{(z')^2}{(a')^2} = 1 \quad (5.16b)$$

However, in practice, during fatigue assessment, the only information of the crack available to the modeller is the extension (location of point p1 and p2 in Figure 5.3a, i.e. s'_1 , s'_2 and l_d) and the depth a' of the surface crack which could be obtained by visual and X-ray-inspections. In such a case, it is necessary to investigate the effect of the parameter ratio l_d / l when FEA is carried out on the life assessment of SSHS joints. l is the crack length as defined in Chapter 5 ($l = s'_1 - s'_2$).

Three mesh models with same geometry and material properties as those of the Specimen II are generated by the proposed mesh generator in Chapter 5. The boundary conditions and the load applied to the models are the same as those of the Specimen II. The crack surfaces are all modelled using the measured crack surface (please refer to Eqn. (5.22)). The location, the crack length and the crack depth of the surface cracks are all the same. That means the cracks have same values of s'_1 , s'_2 , l and a' . The details of the model and the cracks are listed as follows,

$$b_0 = 350.0 \text{ mm}, \quad t_0 = 16.0 \text{ mm};$$

$$b_1 = 200.0 \text{ mm}, \quad t_1 = 16.0 \text{ mm};$$

$$L_{ch} = 4130.0 \text{ mm}, \quad L_{br} = 2165.2 \text{ mm};$$

$$\text{Crack profiles: } a' = 15.33 \text{ mm},$$

$$s'_1 = 26.59 \text{ mm}, \quad s'_2 = 149.41 \text{ mm}, \text{ and } l = 176.0 \text{ mm};$$

$$\text{Crack surface: defined by Eqn. (5.22);}$$

$$\text{The crack is located at corner CP4;}$$

$$\text{Model 1: } l_d / l = 0.5 \quad (l_d = 88.0 \text{ mm});$$

Model 2: $l_d/l = 0.6875$ ($l_d = 121.0$ mm);

Model 3, $l_d/l = 0.875$ ($l_d = 154.0$ mm);

Young's Modulus, $E = 210$ kN/mm²;

Poisson ratio, $\nu = 0.3$;

Load case: AX 70KN + IPB(-3.07)KN + OPB(-0.413)KN;

Boundary condition: Both ends of the chord are fixed. The end of brace is free.

The crack shapes (front) are illustrated in Figure 6.14.

All the models are analyzed by ABAQUS (2004a) and their SIF distributions along the crack front are compared in Figure 6.15. From the figure, it can be seen that the SIF distribution along the crack front is influenced significantly by the shape of the crack. The model with the crack shape $l_d/l = 0.6875$ (which is measured from the actual test results) gives the highest SIF at the deepest point of the crack shape. However, it should be noted that in the model with crack shape $l_d/l = 0.875$ the point where the SIF value is highest is located at the actual measured deepest point instead of the deepest point of the crack shape. The model with crack shape $l_d/l = 0.5$ (which is semi-elliptical) does not have an obvious highest SIF value (Figure 6.15). The SIFs at the crack tips from the three models are also different. This analysis results show that the SIF distribution is sensitive to the crack shape modelled. In general, the more accurate the crack shape modelling is, the more accurate SIF distribution is obtained. However, in practice, it is not easy to get the accurate crack shape because the crack shape evolution may depend on the situations such as joint geometry and mode of loading, etc. Fortunately, from the large-scale test, it is acquired that the deepest point of one crack usually maintains at one point which is the peak hot spot stress location. Hence, FE analysis on the model without crack could be carried out first to get the peak hot spot stress location before the calculation of the SIF at the deepest point. Then the ratio of l_d/l could be known before modelling the crack. Thus accurate SIF value could be obtained from the model with accurate crack shape modelling.

6.2.3 Two Crack Problems

During the large-scale testing, two cracks were found in Specimen II (please refer to Chapter 4). And from the tests it seems that the four corners are detached. That means after the initiation of one crack, the redistribution of the load carrying areas of the joint does not affect the load distribution in other three corners. Hence, when the hot spot stress at other corners is large enough, there is always a chance for another crack's appearance. At this point, models with two cracks are needed in the numerical analysis. However, due to the crack size and crack surface's difference, symmetry method cannot be used. This makes the numerical modelling more complicated. Here, the effect of a second crack on the SIF values of the first crack (main crack) is investigated.

In order to study the influence of the second crack on the first one, models with two cracks are generated using the modelling technique proposed in Chapter 5. The dimension of the model is exactly the same as Specimen II. In the light of the experimental data associated with the main crack profiles, the profile of the first crack in the numerical modelling is identified and the dimension of the second crack is assumed. The location of the second crack is postulated at the adjacent corner as that in Specimen II, as shown in Figure 6.16. In total three cases are studied and the details of the crack profiles are listed as follow;

$$b_0=350.0\text{mm}, \quad t_0=16.0\text{mm};$$

$$b_1=200.0\text{mm}, \quad t_1=16.0\text{mm};$$

$$L_{ch}=4130.0\text{mm}, \quad L_{br}=2165.2\text{mm};$$

$$\text{Young's Modulus, } E = 210 \text{ kN/mm}^2;$$

$$\text{Poisson ratio, } \nu = 0.3;$$

$$\text{Load case: } AX \ 70\text{KN} + IPB(-3.07)\text{KN} + OPB(-0.413)\text{KN};$$

Boundary condition: Both ends of the chord are fixed. The end of brace is free.

Crack profiles:

Case 1: Model 1 (with one crack): $a' = 3.2 \text{ mm}$, $s'_1 = 42.41 \text{ mm}$, $s'_2 = 117.41 \text{ mm}$,
 $l = 75.0 \text{ mm}$ and $l_d = 52.0 \text{ mm}$;

Model 2 (with two cracks):

1st crack: $a' = 3.2 \text{ mm}$, $s'_1 = 42.41 \text{ mm}$, $s'_2 = 117.41 \text{ mm}$,
 $l = 75.0 \text{ mm}$ and $l_d = 52.0 \text{ mm}$;

2nd crack: $a' = 3.2 \text{ mm}$, $s'_1 = 42.41 \text{ mm}$, $s'_2 = 117.41 \text{ mm}$,
 $l = 75.0 \text{ mm}$ and $l_d = 52.0 \text{ mm}$;

Case 2: Model 1 (with one crack): $a' = 8.75 \text{ mm}$, $s'_1 = 14.41 \text{ mm}$, $s'_2 = 144.41 \text{ mm}$,
 $l = 130.0 \text{ mm}$ and $l_d = 85.0 \text{ mm}$;

Model 2 (with two cracks):

1st crack: $a' = 8.75 \text{ mm}$, $s'_1 = 14.41 \text{ mm}$, $s'_2 = 144.41 \text{ mm}$,
 $l = 130.0 \text{ mm}$ and $l_d = 85.0 \text{ mm}$;

2nd crack: $a' = 3.2 \text{ mm}$, $s'_1 = 42.41 \text{ mm}$, $s'_2 = 117.41 \text{ mm}$,
 $l = 75.0 \text{ mm}$ and $l_d = 52.0 \text{ mm}$;

Model 3 (with two cracks):

1st crack: $a' = 8.75 \text{ mm}$, $s'_1 = 14.41 \text{ mm}$, $s'_2 = 144.41 \text{ mm}$,
 $l = 130.0 \text{ mm}$ and $l_d = 85.0 \text{ mm}$;

2nd crack: $a' = 8.75 \text{ mm}$, $s'_1 = 14.41 \text{ mm}$, $s'_2 = 144.41 \text{ mm}$,
 $l = 130.0 \text{ mm}$ and $l_d = 85.0 \text{ mm}$;

Case 3: Model 1 (with one crack): $a' = 13.93 \text{ mm}$, $s'_1 = 5.59 \text{ mm}$, $s'_2 = 144.41 \text{ mm}$,
 $l = 150.0 \text{ mm}$ and $l_d = 100.0 \text{ mm}$;

Model 2 (with two cracks):

1st crack: $a' = 13.93 \text{ mm}$, $s'_1 = 5.59 \text{ mm}$, $s'_2 = 144.41 \text{ mm}$,
 $l = 150.0 \text{ mm}$ and $l_d = 100.0 \text{ mm}$;

2nd crack: $a' = 3.2 \text{ mm}$, $s'_1 = 42.41 \text{ mm}$, $s'_2 = 117.41 \text{ mm}$,
 $l = 75.0 \text{ mm}$ and $l_d = 52.0 \text{ mm}$;

Model 3 (with two cracks):

$$\begin{aligned} 1^{\text{st}} \text{ crack: } & a' = 13.93 \text{ mm}, s'_1 = 5.59 \text{ mm}, s'_2 = 144.41 \text{ mm}, \\ & l = 150.0 \text{ mm and } l_d = 100.0 \text{ mm}; \\ 2^{\text{nd}} \text{ crack: } & a' = 8.75 \text{ mm}, s'_1 = 14.41 \text{ mm}, s'_2 = 144.41 \text{ mm}, \\ & l = 130.0 \text{ mm and } l_d = 85.0 \text{ mm}; \end{aligned}$$

Model 4 (with two cracks):

$$\begin{aligned} 1^{\text{st}} \text{ crack: } & a' = 13.93 \text{ mm}, s'_1 = 5.59 \text{ mm}, s'_2 = 144.41 \text{ mm}, \\ & l = 150.0 \text{ mm and } l_d = 100.0 \text{ mm}; \\ 2^{\text{nd}} \text{ crack: } & a' = 13.93 \text{ mm}, s'_1 = 5.59 \text{ mm}, s'_2 = 144.41 \text{ mm}, \\ & l = 150.0 \text{ mm and } l_d = 100.0 \text{ mm}; \end{aligned}$$

The crack surfaces are defined by Eqn (5.22);

The crack locations are illustrated in Figure 6.16.

The crack profiles are selected under such rule as that the crack is set small, middle and large in sequence, so as to find out the second crack effect of different size cracks. The models are analysed using ABAQUS (2004a).

The results of the SIF distribution along the crack front are compared in Figures 6.17 to 6.19. The comparisons show that when the first crack is relatively small, there are almost no difference between the models with one crack and two cracks. That means the second crack has no effect on the SIF of first crack (Figure 6.17). When the first crack becomes deeper and longer, the effect of the second crack becomes more and more obvious (Figures 6.18 and 6.19). The model with two cracks has higher SIF. That means the stiffness of the structure is reduced because of the appearance of the second crack when the first crack is long and deep enough. However, the effect of the second crack is quite small. In case 3, the biggest SIF difference of the model with one crack and two cracks is less than 10%. Hence, when the crack length is less than quarter of the circumference of the intersection, the influence of the second crack can be ignored.

However, when the crack is quite long, the influence of the second crack on the first one should be considered.

6.3 FE ANALYSIS ON WELD SIZE EFFECT

Bowness and Lee (1995b) believed that the weld thickness in the mesh models has an effect on accurately obtaining the stress intensity factors of the crack in the tubular joints. Huang (2002) also investigated the influence of the weld thickness and he found that the weld thickness had little influence to the stress intensity factor results of the crack at the deepest point. Huang (2002) also pointed out that more investigation needs to be done on this problem. Some welding standards, for example API (1993) and AWS (2000), all give the suggestion of the minimum standard profile of weld to the different kinds of joints as presented in Appendix A. The problem is that the weld thickness of the joints is variable in practice though it needs to satisfy the minimum thickness suggested by codes. Thus, the influence of the weld thickness modelling in the models to the stress intensity factor results needs to be studied. Here, one square-to-square hollow section (SSHS) T-joint with different weld size is modelled and investigated.

Chapter 5 has described the details of the weld modelling. For SSHS T-joint, the weld size is decided by the parameter “W” which is defined as “F” in AWS (2000). The details of the model and weld size are listed as follows,

$$\begin{aligned} b_0 &= 350.0\text{mm}, & t_0 &= 16.0\text{mm}; \\ b_1 &= 200.0\text{mm}, & t_1 &= 16.0\text{mm}; \\ L_{ch} &= 4130.0\text{mm}, & L_{br} &= 2165.2\text{mm}; \\ W &= 0.5t_1, 0.75t_1, 1.0t_1; \end{aligned}$$

Crack profile¹: $a' = 3.2$ mm, $s'_1 = 42.41$ mm, $s'_2 = 117.41$ mm, $l = 75.0$ mm and $l_d = 52.0$ mm;

Crack profile²: $a' = 8.75$ mm, $s'_1 = 14.41$ mm, $s'_2 = 144.41$ mm, $l = 130.0$ mm and $l_d = 85.0$ mm;

Crack profile³: $a' = 13.93$ mm, $s'_1 = 5.59$ mm, $s'_2 = 144.41$ mm, $l = 150.0$ mm and $l_d = 100.0$ mm;

The crack surface is defined by Eqn. (5.22);

Load case 1: 6 MPa tensile stress is applied at the end face of the brace;

Load case 2: 5KN IPB is applied at the end of the brace;

Load case 3: (-5)KN OPB is applied at the end of the brace;

Young's Modulus: $E = 210$ kN/mm²;

Poisson ratio: $\nu = 0.3$

Boundary condition: Both ends of the chord are fixed. The end of brace is free.

The crack is located at corner CP4.

The model with different crack depth and different weld size (W) are generated. The purpose of this is to investigate the influence of the weld size to the stress intensity factor results of the crack with different depth. All the models are then analyzed in ABAQUS (2004a) under the three load cases. The SIF results of the deepest points are listed in Table 6.3. From the table, it clearly shows that the weld size has effect on the SIF (K_e) of the deepest point. Generally, the SIF (K_e) decreases when the weld size (W) increases under all the load cases. It also can be seen that the influence of the weld on the SIF (K_e) values is different corresponding to different crack depth. The influence of weld size on SIF (K_e) is inversely proportional to the crack depth. When the crack is shallow ($a' = 3.2$ mm), the influence of the weld size to the SIF (K_e) is significant, and when the crack depth increases ($a' = 8.75$ mm and $a' = 13.93$ mm), the influence of the weld size becomes smaller. The influence of the weld size (W) on K_I is similar to that on SIF (K_e) but the influence of the weld size on K_{II} and K_{III} is not obvious. From above comparison, it also can be known that the model with the minimum weld size (W)

will generate maximum SIF values. Hence, in the numerical analysis, modelling the joint with the minimum weld size provided by the AWS (2000) will give conservative SIF results.

6.4 FE ANALYSIS ON CONTACT PROBLEMS

The crack surfaces on the joints subjected to some kinds of loads may contact and penetrate each other due to the compressive stresses. Under this situation, contact surfaces should be defined on the crack surfaces to avoid this problem. Prior to this definition, the influence of the contact surface modelling on the crack surfaces to the stress intensity factor results of the crack should first be investigated. In this study, a SSHS T-joint model, which is also the model of Specimen II, is used to study this influence. The details of the model are listed as follows:

$$b_0=350.0\text{mm}, \quad t_0=16.0\text{mm};$$

$$b_1=200.0\text{mm}, \quad t_1=16.0\text{mm};$$

$$L_{ch}=4130.0\text{mm}, \quad L_{br}=2165.2\text{mm};$$

$$\text{Crack profiles: } a' = 8.75 \text{ mm}, \quad s'_1 = 45.59 \text{ mm}, \quad s'_2 = 144.41 \text{ mm}, \quad l = 195.0 \text{ mm} \text{ and}$$

$$l_d = 140.0 \text{ mm};$$

The crack surface is defined by Eqn. (5.22);

The crack is located at corner CP4;

Young's Modulus, $E = 210 \text{ kN/mm}^2$;

Poisson ratio, $\nu = 0.3$;

Load case 1: 6MPa tensile stress is applied at the end face of the brace;

Load case 2: IPB5KN is applied at the end of the brace;

Load case 3: IPB (-5KN) is applied at the end of the brace;

Boundary condition: Both ends of the chord are fixed. The end of brace is free.

The mesh models with and without the contact surfaces modelling on the crack surfaces are generated respectively. These two models are then analyzed by ABAQUS (2004a) under the load cases listed above.

In case of Load case 1, the comparison of the stress intensity factor results of the models with and without contact surface definition is presented in Figure 6.20. It can be seen that under this load condition, no contact occurs on the crack surfaces because all the K_I values are positive. The results of the two models are almost the same. That means the contact surface definition on the crack surface causes little influence to the convergence of the results.

The Load case 2 is selected in such a way that most of the crack extension is under tensile stress and Load case 3 is that most of crack is under compression. The comparison of the stress intensity factor results of the model with and without contact surface definition is presented in Figures 6.21 and 6.22. From Figure 6.21, it can be seen that, the negative K_I values in the model without contact surface definition illustrate that part of the crack surfaces contacted and penetrated each other when the models were subjected to the Load case 2. This also can be known from the output messages of the model with the contact surface modelling. It also can be seen that the results of the model with and without contact surface definition are almost the same in the region out of the contact area, but the results are much different in the region in the contact area. From the Figure 6.22, it can be seen that most part of the crack surfaces contacted when the models were subjected to Load case 3. The results are different in both contact area and non-contact area. That means the contact surface has affected the non-contact region. This will depend on the load case and the crack profiles. Hence, the contact problem should be considered in the FE models for a mixed mode crack.

6.5 CONCLUDING REMARKS

In this chapter, five factors that may affect the SIF results of one crack due to the difference in the numerical modelling are investigated i.e. crack surface, crack shape, two crack problems, contact problems and weld size influence. From the investigation, it is known that the crack surface has significant effect on the SIF values at the deepest point. The crack surface cannot be represented by a straight line or a single-variable equation. It is a double-curved surface and a double-variable must be used to define the crack surface in both crack depth direction and crack length direction. One general crack profile equation is suggested based on the full-scale test results and the numerical comparison. Both the SIF at the deepest and along the crack front are compared between the models with actual measured crack surface and proposed crack surface. The good agreements show that the proposed crack surface equation is good enough for the SSHS fatigue analysis.

In order to get one crack shape that can be used in practice, two-quarter-ellipse is proposed. In the investigation of the crack shape influence, the model with different l_d/l ratio (Figure 6.11) is generated. The crack shape (l_d/l) affects largely both on the SIF values at the deepest point and the SIF distribution along the crack front (Figure 6.12). The location of the maximum SIF shifts when different l_d/l ratio is used. However, it was observed that the two SIF values are almost same at the deepest point where the peak HSS is obtained. One benefit from this finding is that, in practice, HSS analysis can be carried out first to get the accurate crack shape modelling.

In the investigation of the influence of the second crack on the development of the first one, it was found that when the main crack and the second crack are very small, the model can be simplified to modelling the joint with one main crack, since the influence of the second crack is quite small at this condition. Unfortunately, when both the main crack and the second crack are very deep and long, such as more than quarter of the brace circumference, the second crack cannot be ignored and the model with two cracks must be analyzed.

The weld size (W) has significant influence on the SIF (K_e) results. When the weld size (W) increases, the SIF (K_e) value at the deepest point decreases. The shallower the crack is, the more significant the influence of the weld size (W) on the SIF (K_e) value. The weld size (W) has the similar effect on K_I , but has little effect on K_{II} and K_{III} . Hence, in the modelling of weld size, AWS minimum values can be used since the model can give the most conservative SIF (K_e) results.

In the contact problem, model with cracks under three loads is investigated. The case under AX load shows that the contact surface definition on the crack surface causes little influence to the convergence of the results. When the crack is under comparison, contact surface must be defined since the SIF distribution is very different in the contact area. Even in the region out of the contact area, the SIF result is affected by the contact surface definition.

Concisely, the investigations in this chapter clearly show that in the numerical analysis, some factors should be considered and some can be ignored under certain conditions. The main value of such an observation is that it can be used to simplify the modelling and ensure the accuracy of the numerical results.

Figure 6.1 Straight line crack surfaces studied in crack surface modelling

Figure 6.2 SIF of the deepest point from the model with different straight line crack surface in Specimen I

Figure 6.3 SIF of the deepest point from the model with different straight line crack surface in Specimen II

Figure 6.4 SIF of the deepest point from the model with different straight line crack surface in Specimen III

Figure 6.5 SIF of the deepest point from the model with different straight line crack surface in Specimen IV

Figure 6.6 SIF comparisons at the deepest point for the model with proposed surface and the measure surface in Specimen I

Figure 6.7 SIF comparisons at the deepest point for the model with proposed surface and the measure surface in Specimen II

Figure 6.8 SIF comparisons at the deepest point for the model with proposed surface and the measure surface in Specimen III

Figure 6.9 SIF comparisons at the deepest point for the model with proposed surface and the measure surface in Specimen IV

Figure 6.10 SIF comparisons along crack front of the model with proposed surface and measured surface in Specimen I

Figure 6.11 SIF comparisons along crack front of the model with proposed surface and measured surface in Specimen II

Figure 6.12 SIF comparisons along crack front of the model with proposed surface and measured surface in Specimen III

Figure 6.13 SIF comparisons along crack front of the model with proposed surface and measured surface in Specimen IV

Figure 6.14 Crack shapes used in the analysis of crack shape effect

Figure 6.15 SIF distributions along the crack front of the models with different crack shape (l_d/l)

Figure 6.16 Location of the two cracks in the analysis of two crack problems

Figure 6.17 SIF comparison of the model with one crack and two cracks (Case 1)

Figure 6.18 SIF comparison of the model with one crack and two cracks (Case 2)

Figure 6.19 SIF comparison of the model with one crack and two cracks (Case 3)

Figure 6.20 K_I results comparison between the models with and without contact surface definition under load case 1 (AX load)

Figure 6.21 K_I results comparison between the models with and without contact surface definition under load case 2 (IPB 5KN)

Figure 6.22 K_I results comparison between the models with and without contact surface definition under load case 2 (IPB (-5KN))

Table 6.1 Crack profiles in crack surface analysis

Crack profile	a' (mm)	s'_1 (mm)	s'_2 (mm)	l (mm)	l_d (mm)
	Specimen I				
1	4.46	80.50	150.50	70	40
2	9.95	63.50	167.50	104	57
3	15.09	60.50	170.50	110	60
Load(KN)	AX(150)+IPB(5.82)+OPB(-3.97)				
	Specimen II				
1	4.96	19.41	144.41	125	85
2	8.75	14.41	144.41	130	85
3	13.93	5.59	144.41	150	100
Load(KN)	AX(75)+IPB(-3.07)+OPB(-0.413)				
	Specimen III				
1	3.99	68.36	136.36	68	30
2	8.41	58.36	144.36	86	40
3	13.03	40.36	151.36	111	58
Load(KN)	IPB(-4.17)+OPB(-3.8)				
	Specimen IV				
1	3.13	57.49	127.49	70	40
2	8.18	27.49	137.49	110	70
3	14.88	27.51	137.49	165	125
Load(KN)	AX(70)+IPB(1.4)+OPB(-1.693)				
Note: Positive loads are tension loads, and negative loads are compression loads					

Table 6.2 Influence of the weld size to the SIF results of the crack

Load	Under AX			Under IPB			Under OPB		
W	0.5t ₁	0.75t ₁	1.0t ₁	0.5t ₁	0.75t ₁	1.0t ₁	0.5t ₁	0.75t ₁	1.0t ₁
Model with crack profile ¹									
K _e	14.14	12.76	11.19	21.19	18.12	15.80	17.61	16.36	14.51
K _I	14.08	12.68	11.08	21.19	17.93	15.57	17.53	16.27	14.38
K _{II}	1.05	1.18	1.38	1.42	1.5	1.79	1.63	1.69	1.95
K _{III}	-0.68	-0.61	-0.57	-0.87	-1.81	-1.69	-0.51	-0.35	-0.34
Model with crack profile ²									
K _e	13.42	12.61	11.74	19.88	18.06	16.76	16.95	15.59	14.61
K _I	13.36	12.53	11.65	19.87	17.99	16.66	16.91	15.51	14.51
K _{II}	0.99	1.13	1.21	0.75	1.30	1.42	1.16	1.51	1.67
K _{III}	-0.76	-0.61	-0.72	0.05	-0.78	-1.01	-0.15	-0.22	-0.39
Model with crack profile ³									
K _e	12.26	11.36	10.83	17.09	16.22	15.55	14.72	13.38	12.72
K _I	12.04	11.22	10.72	16.76	15.89	15.32	14.46	13.19	12.60
K _{II}	-2.08	-1.64	-1.30	-3.37	-3.13	-2.61	-2.67	-2.20	-1.74
K _{III}	-0.86	-0.53	-0.63	0.19	0.75	0.52	-0.57	-0.01	-0.09

CHAPTER 7

PART-THROUGH CRACK FATIGUE LIFE PREDICTION

7.1 INTRODUCTION

Fatigue failure of hollow section joints due to repeated loads has been recognized for over a few decades. Studies have ranged from the microscopic dislocation mechanism to phenomenological material behaviour to full scale structure testing and analysis. Focused on the fatigue failure of large-scale welded hollow section joints, many efforts have been invested into various sub-topics, such as stress concentration factors (SCFs) around the brace-chord intersection, the calculation of hot spot stress (HSS) around the intersection, stress distribution along the crack front, calculation of stress intensity factors (SIFs), material properties etc. All of these studies are conducted through full-scale testing and finite element simulation conducted under some assumptions. The objective of all these fatigue analyse, calculations, testing and assumptions is to obtain an acceptable combination of load and life – to predict the fatigue life of hollow section joints under a certain cyclic load.

The majority of investigations about fatigue process have been concerned with the phenomenological aspect of fatigue failure, i.e. estimating the total life of hollow section joints under a specific load for the past few decades. Up to today, various S-N curves have been developed and widely adopted in many fatigue design codes (Design Guide CIDECT, API, AWS etc). However, going deeper the fatigue process, fatigue life for a component should involve two distinct problems: initiation (and subsequent growth) of surface cracks at highly stressed notches in inherently defect-free material; and, propagation of cracks from inherent defects in smooth regions of large material

volume with moderate stress levels. In this type of approach no distinction is made between the crack initiation and propagation phases. That's why the topics on the crack driving force (stress intensity factor) and crack propagation rate are issued. These make preparations for the life prediction of the crack propagation phases.

In Chapter 5, a finite element modelling technique is provided. Its validity is proved against the experimental results conducted in Chapter 3 and 4. The model can generate quite accurate stress intensity factors (SIF) of a square-to-square hollow section (SSHS) joints. Once the crack driving force (stress intensity factor) is obtained, the life prediction for a part-through crack can be performed. In this study, a life prediction method for a part-through crack (PTC) is proposed. Its validity is illustrated by the four full-scale fatigue tests results.

7.2 SCOPE OF FATIGUE LIFE PREDICTION

The behaviour of fatigue crack propagation in metal can be divided into three regions as shown in Figure 2.8, which is repeated as Figure 7.1 (Naess, 1985) here. The behaviour in Region I exhibits a fatigue-crack-propagation threshold, ΔK_{th} , which corresponds to the stress-intensity-factor range below which cracks do not propagate under cyclic-stress fluctuation. An analysis of experimental results published on nonpropagating cracks shows that conservative estimates of ΔK_{th} for various steels subjected to various stress ratios, R , can be predicted from,

$$\begin{aligned}\Delta K_{th} &= 6.4(1 - 0.85R) \text{ for } R > +0.1 \\ \Delta K_{th} &= 5.5 \text{ ksi}\sqrt{\text{in}} \text{ for } R < +0.1\end{aligned}\tag{7.1}$$

where ΔK_{th} is in $\text{ksi}\sqrt{\text{in}}$.

Eqn. (7.1) indicates that the fatigue-crack propagation threshold for steels is primarily a function of the stress ratio and is essentially independent of chemical composition or mechanical properties.

The behaviour in Region II represents the fatigue-crack-propagation behaviour above ΔK_{th} , which can be represented by Paris Law,

$$\frac{da}{dN} = C(\Delta K)^m \quad (7.2)$$

where a = crack depth

N = number of cycles,

ΔK = stress intensity factor range,

C and m are material constants.

In Region III the fatigue-crack propagation per-cycle is higher than the prediction in Region II. Acceleration of fatigue-crack propagation rates that determines the transition from region II to region III occurs and it is thought to be caused by the superposition of ductile-tear or brittle mechanisms onto the mechanism of cyclic subcritical-crack extension, which leaves fatigue striations on the fracture surface.

In this study, the life prediction is strictly limited in Region II in the sense that the continuum fracture mechanics can be applied.

7.3 ANALYSIS PROCEDURE

There are four pieces of major material property information used in a fracture mechanics fatigue life prediction: (1) the loads and stresses, (2) the material properties, (3) the crack driving force and (4) the crack size after inspection. Utilizing these

information, crack growth progression can be calculated as a function of load cycles, as shown schematically in Figure 7.2.

7.3.1 Load and Stresses

To calculate the crack driving forces, the first necessary items are the structural loadings, which should consist of both applied and residual stresses. One important observation is that, unlike conventional crack initiation calculations, the stress distribution along potential crack paths needs to be known as well as the surface stresses. It is relatively straightforward, at times, to compute the stresses in an unflawed structure due to applied loadings. The residual fabrication stresses are unknown in a majority of the structures. However, in the stress calculation of a hollow section joints in this study, the residual fabrication stresses are usually ignored since residual stresses only have large effect on the crack initiation instead of the crack propagation which is the focus of this study.

7.3.2 Crack Driving Force (Stress Intensity Factor)

Knowing the uncracked stress field, we must compute the crack driving force (that is, the stress intensity factors as the cracks grow through structure). There are various handbooks available with SIF-solution for most common crack shapes, simple structures and loading types. There are also several computer codes, such as ABAQUS, MARC, DIANA etc., and also various finite element models to compute the SIF for various crack shapes, structures and load types. However, specific engineering modelling is still frequently required to perform an appropriate SIF calculation. The extent of the engineering modelling will depend on the complexity of the cracked structure and the accuracy required. From previous discussion in Chapter 5 and 6, a finite element modelling technique which can generate square-to-square hollow section (SSHS) joints with complicate crack surface was proposed and verified by

experimental results. Hence, in this study, the proposed modelling technique and ABAQUS (2004) are used to compute the crack driving force (SIF).

7.3.3 Material Properties

In general, three types of data of materials crack are needed to predict fatigue life. Firstly, the crack growth rate which is defined as a function of the range (ΔK) of crack tip SIF and mean value (K_{mean}) must be obtained. The relationship is theoretically expressed as,

$$\frac{da}{dN} = f(\Delta K, K_{mean}, \text{loading rate, environment, etc}) \quad (7.3)$$

Where ΔK must be defined in the predict model in the same manner as was used to report the crack growth rate data. That means when the structure undergoes tension-compression cycling ($R = \frac{\sigma_{min}}{\sigma_{max}} < 0$), the calculation of ΔK , whether or not the compression loading is used, should be consistent with the manner in which ΔK was calculated in the reported crack growth rate data. As this PTC life prediction is limited to the crack propagation phase in Region II as shown in Figure 7.1, Paris law is adopted as the function between crack growth rate and the SIF range (ΔK).

Secondly, (K_{TH}), the growth threshold of materials fatigue crack below which cracks will not grow must be known. As previously mentioned, this PTC life prediction is limited to the Region II, which means the crack has a stable growth in this region, the SIF (K) is larger than K_{TH} .

Finally, the materials fracture toughness (K_c) must be obtained in general life prediction to get the upper-limit of the fatigue life prediction, because when the SIF is

larger than K_c , unstable crack growth which is no longer fatigue mechanism but fracture mechanism will occur. In this study, the upper-limit of the fatigue life prediction is decided by one terminal crack size.

7.3.4 PTC Fatigue Life Prediction

In the situation discussed in Section 7.2 where linear fracture mechanics method are applicable, crack growth rate can be expressed as a function of the SIF at the crack front. Then once the part-through surface crack driving force, the stress distribution is characterized, accurate fatigue life predictions can be made for this class of crack geometries.

In order to estimate the fatigue life of the cracked joint, one may rearrange Eqn. (7.2) so that dN becomes the object and integrating both sides of the equation and obtains:

$$\int dN = \int \frac{da'}{C(\Delta K)^m} \quad (7.4)$$

Suppose that an approximation K_e for ΔK is available for a' within a given range $[a'_{initial}, a'_{final}]$ such that $0 \leq a'_{initial} < a'_{final}$. Then with the presence of Eqn. (7.4), the residual life, $N_r(a')$, which is the number of load cycles for the crack to penetrate from a curved depth of a' to a'_{final} , can be estimated as,

$$N_r(a') = \int_{a'}^{a'_{final}} \frac{da'}{C(K_e)^m} \quad \text{for } a'_{initial} \leq a' \leq a'_{final} \quad (7.5)$$

As K_e is computed by numerical simulation, its value is only defined at some selected values of a' in the range $[a'_{initial}, a'_{final}]$. Hence, numerical integration is required to

compute Eqn. (7.5). The accuracy of the residual life predicted by Eqn. (7.5) depends on the accuracy of K_e used.

7.3.5 Conservatism of Life Prediction

Once there is one method to predict the fatigue life of the joint, the conservatism of the method is required to be evaluated. The conservatism of the predicted residual life can be assessed by the factor of safety of the prediction, FOS_N , which is defined as the ratio between $N_{act}(a')$, the actual number of cycle recorded for the crack to penetrate from a' to a'_{final} , and $N_r(a')$. That is

$$FOS_N(a') = \frac{N_{act}(a')}{N_r(a')} \quad (7.6)$$

A value of $FOS_N < 1.0$ indicates that prediction is not conservative. In practice, a slightly conservative result of FOS_N is preferred.

7.4 EXAMPLES

To illustrate the procedure of the aforementioned fracture mechanics fatigue analysis, four examples which are the specimens tested in Chapter 4 are described here.

Example 1 (Specimen I):

Load-frequency: 0.5 HZ;

Constant-amplitude-sinusoidal load, AX150(KN)+IPB3(KN)+OPB(-3)(KN) and

$$R = \frac{\sigma_{min}}{\sigma_{max}} = 0;$$

Material constant, $C = 8.02 \times 10^{-12}$, and $m = 2.92$;

Initial crack depth and terminal crack depth is listed in Table 7.1.

Example 2 (Specimen II):

Load-frequency: 0.25 HZ;

Constant-amplitude-sinusoidal load, AX75(KN)+IPB(-3)(KN)+OPB(-3)(KN) and

$$R = \frac{\sigma_{\min}}{\sigma_{\max}} = 0;$$

Material constant, $C = 8.02 \times 10^{-12}$, and $m = 2.92$;

Initial crack depth and terminal crack depth is listed in Table 7.2.

Example 3 (Specimen III):

Load-frequency: 0.5 HZ;

Constant-amplitude-sinusoidal load, AX70(KN)+IPB(-3)(KN)+OPB(-3)(KN) and

$$R = \frac{\sigma_{\min}}{\sigma_{\max}} = 0;$$

Material constant, $C = 8.02 \times 10^{-12}$, and $m = 2.92$;

Initial crack depth and terminal crack depth is listed in Table 7.3.

Example 4 (Specimen IV):

Load-frequency: 0.5 HZ;

Constant-amplitude-sinusoidal load, AX70(KN)+IPB(-3)(KN)+OPB(-3)(KN) and

$$R = \frac{\sigma_{\min}}{\sigma_{\max}} = 0;$$

Material constant, $C = 8.02 \times 10^{-12}$, and $m = 2.92$;

Initial crack depth and terminal crack depth is listed in Table 7.4.

By using Eqn. (7.5), the SIFs obtained by using different models are employed to predict the residual life of the four examples listed above. Figures 7.3 to 7.6 compare the results obtained by using geometrical models with different crack surface (measured, proposed, constant $\theta_s(z') = \pi/3$ and $\pi/4$) with the actual ACPD measurements. The values of a'_{initial} and a'_{final} employed are the values corresponding to the range of

a' for the geometrical model used in the SIF calculations and are listed in Tables 7.1 to 7.4. Results for the FOS_N obtained using the prediction from Figures 7.3 to 7.6 are shown in Figures 7.7 to 7.10 respectively. From Figures 7.3 to 7.10, it can be concluded that,

- (1) In general, all the models gave conservative predictions when compared with the actual measurements. The only exception is in *Example 1* (Specimen I), the proposed model and the model based on exact geometry gave almost exact but slightly underestimated residual life for $8\text{mm} \leq a' \leq 11\text{mm}$.
- (2) Models based on constant $\theta_s(z') = \pi/3$ and $\pi/4$ appears to be too conservative, especially for Specimen III. Hence, despite that they are simpler than the proposed model, they are not suitable to be used in practice.
- (3) As expected, the model based on exact geometry gave the most accurate prediction with values of FOS_N closest to unity. The proposed model gave prediction slightly more conservative than the exact model. In fact, for Specimens I, II and IV, the FOS_N for the proposed model are only slightly higher than the results from the exact model. For Specimen III, slight divergence of the FOS_N is observed when a' is close to a'_{final} . Hence, it can be concluded that the proposed model led to practical prediction of residual life.

The results of the life prediction from the model with the proposed surface are also listed in Table 7.1 to 7.4. It can be seen clearly that the proposed model is accurate enough for the life prediction.

7.5 CONCLUDING REMARKS

In this chapter, one flow chart of the procedure to predict the fatigue life of a hollow section joint with part-through cracks is presented. This life prediction can be made using the modelling technique proposed in Chapter 5 and Paris Law. The conservatism

of the life prediction is evaluated. Four examples are listed based on the four fatigue tests carried out in Chapter 4 to illustrate the life prediction procedure. The results also show that the proposed model can provide quite accurate SIF calculation to ensure that the predicted fatigue life is closer to the actual condition.

Figure 7.1 Three stages of fatigue crack growth

Figure 7.2 Flow chart of fracture mechanics (LEFM) life prediction system

Figure 7.3 Prediction of residual fatigue life, Specimen I (Example 1)

Figure 7.4 Prediction of residual fatigue life, Specimen II (Example 2)

Figure 7.5 Prediction of residual fatigue life, Specimen III (Example 3)

Figure 7.6 Prediction of residual fatigue life, Specimen IV (Example 4)

Figure 7.7 $FOS_N(a')$ for the prediction of residual fatigue life, Specimen I (Example 1)

Figure 7.8 $FOS_N(a')$ for the prediction of residual fatigue life, Specimen II (Example 2)

Figure 7.9 $FOS_N(a')$ for the prediction of residual fatigue life, Specimen III (Example 3)

Figure 7.10 $FOS_N(a')$ for the prediction of residual fatigue life, Specimen IV (Example 4)

Table 7.1 Life prediction from proposed model for Example 1 (Specimen I)

$a'_{initial}$ (mm)	a'_{final} (mm)	ΔK (MPa $\times$ m ^{1/2})	$N_r(a')$ (cycle)	$N_{act}(a')$ (cycle)	$FOS_N(a')$
2.104	19.328	21.27	208807	226512	1.08
2.926	19.328	22.12	195949	210011	1.07
4.456	19.328	22.94	174487	187508	1.07
6.259	19.328	23.51	151384	155508	1.03
8.077	19.328	24.07	129661	121507	0.98
9.951	19.328	24.40	108472	105506	0.97
11.338	19.328	24.49	93189	89506	0.96
13.267	19.328	23.96	71331	75506	1.06
15.085	19.328	23.64	49656	59005	1.19
17.005	19.328	23.92	26719	34502	1.29

Note: $N_r(a')$ = Predicted load cycles

$N_{act}(a')$ = Tested load cycles recorded by ACPD

$FOS_N(a') = N_{act}(a') / N_r(a')$, factor of safety

Table 7.2 Life prediction from proposed model for Example 2 (Specimen II)

$a'_{initial}$ (mm)	a'_{final} (mm)	ΔK (MPa $\times$ m ^{1/2})	$N_r(a')$ (cycle)	$N_{act}(a')$ (cycle)	$FOS_N(a')$
1.752	19.384	21.82	217371	339012	1.56
2.352	19.384	23.03	208828	323511	1.55
3.200	19.384	23.94	198296	308261	1.55
4.965	19.384	24.90	178744	277260	1.55
6.049	19.384	25.45	167768	262009	1.56
7.744	19.384	25.50	151198	246509	1.63
8.755	19.384	25.30	141242	231008	1.64
11.339	19.384	24.59	114354	215757	1.89
13.927	19.384	22.90	83082	184756	2.22
15.331	19.384	22.20	63460	153754	2.42
17.850	19.384	21.33	24402	76753	3.15

Note: $N_r(a')$ = Predicted load cycles

$N_{act}(a')$ = Tested load cycles recorded by ACPD

$FOS_N(a') = N_{act}(a') / N_r(a')$, factor of safety

Table 7.3 Life prediction from proposed model for Example 3 (Specimen III)

$a'_{initial}$ (mm)	a'_{final} (mm)	ΔK (MPa $\times$ m ^{1/2})	$N_r(a')$ (cycle)	$N_{act}(a')$ (cycle)	$FOS_N(a')$
1.424	20.028	18.97	415541	559188	1.35
2.589	20.028	20.66	391605	537091	1.37
3.986	20.028	20.62	366370	508881	1.39
5.951	20.028	20.32	329989	480670	1.46
8.408	20.028	19.38	280050	422715	1.51
10.785	20.028	19.35	228316	366291	1.60
13.029	20.028	18.80	177213	281657	1.59
15.787	20.028	18.56	110526	168357	1.52
18.431	20.028	18.10	42932	55967	1.30

Note: $N_r(a')$ = Predicted load cycles

$N_{act}(a')$ = Tested load cycles recorded by ACPD

$FOS_N(a') = N_{act}(a') / N_r(a')$, factor of safety

Table 7.4 Life prediction from proposed model for Example 4 (Specimen IV)

$a'_{initial}$ (mm)	a'_{final} (mm)	ΔK (MPa $\times$ m ^{1/2})	$N_r(a')$ (cycle)	$N_{act}(a')$ (cycle)	$FOS_N(a')$
1.275	16.432	11.17	1271651	1903153	1.50
3.135	16.432	12.62	1099978	1706130	1.55
5.089	16.432	12.99	957325	1508199	1.58
6.559	16.432	12.73	851422	1367141	1.61
8.178	16.432	12.56	729047	1197420	1.64
9.809	16.432	12.17	597275	999943	1.67
11.302	16.432	12.12	470300	731664	1.56
12.810	16.432	11.56	331729	519391	1.57
14.883	16.432	11.60	129239	152087	1.18

Note: $N_r(a')$ = Predicted load cycles

$N_{act}(a')$ = Tested load cycles recorded by ACPD

$FOS_N(a') = N_{act}(a') / N_r(a')$, factor of safety

CHAPTER 8

CONCLUSIONS AND RECOMMENDATIONS

8.1 INTRODUCTION

In this study, four large-scale SSHS T-joints with different geometrical parameters are tested under both static and fatigue loadings. During the static tests, stress concentration factor (SCF) and hot spot stress (HSS) distributions are investigated. In fatigue tests, constant amplitude combined loads are applied to the specimens respectively, while the crack initiation and propagation are monitored by alternating current potential drop (ACPD) technique. The specimens are split along the crack surface after the fatigue tests and the crack surfaces and crack shapes are measured by clay modelling technique. These test results provide fundamental and valuable information for the numerical modelling.

Subsequently, a new mesh modelling method is proposed for SSHS T-joints with surface cracks located at arbitrary location along chord weld toe based on the information provided by the fatigue tests. The crack surface is defined as a bi-variable function along the crack length and crack depth. Then a tailor-made mesh generator is developed for producing the complete finite element mesh model. This technique can be realized by sub-dividing the entire structure into distinct zones. In each zone, the mesh is generated separately. After the meshes of all the zones are completed, the complete model is obtained by merging the mesh of every zone. It has been proved to be efficient and effective in producing different quality mesh at different zones. Hence, coarse mesh can be generated in the far field and fine mesh in the zone where the stress

gradient is high. After the mesh is generated, test results are used to validate the numerical model.

After the finite element models have been validated against the experimental tests, the numerical modelling is used to investigate the effect factors that may influence the stress distribution around the crack front (stress intensity factors), i.e. crack profiles, weld size and contact problems. Based on the numerical analysis, a general crack surface equation is proposed.

Finally, a life-prediction procedure is introduced. This procedure is illustrated by the four fatigue test results and its validity is confirmed.

8.2 CONCLUSIONS

8.2.1 Experimental studies

Four full-size SSHS T-joints with different geometrical parameters are subjected to both static and fatigue loadings. In the static tests, both basic loads and combined loads are applied to study the SCF and HSS distributions along the chord-brace intersection. The static test results obtained indicate that peak HSS occurs at the corners of the chord-brace intersection. The measured SCFs are different from that predicted by the existing guideline (CIDECT). In CIDECT guide, same equation is provided for points A and E (please refer to Figure 2.3), but the experimental results show that the values predicted by the equations for point E are too high and different equations should be used at points A and E. The results also show that the superposition method can be used to predict the HSS distribution for combined loadings. Furthermore, it seems that a higher stress-strain conversion factor (S_{nf}) of 1.2 should be used for the case involving RHS joints.

In the fatigue tests, the four specimens are subjected to combined loadings with different component ratio. ACPD technique is used to record the detailed information of the crack propagation carefully. And the clay modelling technique is adopted to investigate the crack surfaces and crack shapes after the fatigue tests. The fatigue tests demonstrate that under cyclic loading, cracks initiate at the corners of the brace chord intersections and propagate in a direction parallel to the chord wall, even without any OPB load. Results obtained by the ACPD technique and post-failure measurements show that the crack has a 3D unsymmetrical surface with curved crack profile at any cross section. The crack growth rate at the deepest point may not increase monotonically with the number of load cycles applied. Significantly long and advanced crack appears before a through-thickness crack (TTC) is formed. Furthermore, secondary cracks and branches, which seldom appear in CHS joints, are formed near the end of the tests. The experimental results are also used to validate the S-N design curves suggested in IIW (1998) and CIDECT guide (Zhao et al, 2000). The S-N data obtained from the experimental tests show that the S-N design curves (IIW, 1998 and Zhao et al, 2000) can be used safely to predict the total fatigue life of the SSHS T-joints. Based on the test results, it is suggested that the fatigue life of a RHS joint should be considered as the number of load cycles when the curved crack depth is equal to the thickness of the chord.

8.2.2 Numerical modelling

In the study of fatigue performance of SSHS T-joints, stress intensity factor (SIF) is an important parameter which is frequently used by designers to predict the integrity and residual life of hollow section joints. In order to obtain accurate SIFs along the crack front of any 3D crack located at the HSS location of joints, it is crucial to model the surface crack and the weld toe as realistically as possible. Due to this requirement, a detailed surface crack and weld toe modelling has been proposed based on the results from the testing of four full-scale SSHS T-joints in this study. The crack surface is a 3D unsymmetrical surface with curved crack profile at any cross section. The crack

shape (front) is modelled as a unsymmetrical bi-elliptical curve. Then a new mesh generator for SSHS joints with surface crack is developed whereby five types of elements are used to model the surface crack region accurately. This method is realized by dividing the entire structure into several sub-zones where each zone consists of different types of elements and mesh densities. This method has a distinct advantage of controlling the mesh quality, and most importantly the aspect ratio of the elements along the crack front. When the meshes of all sub-zones are generated automatically and completely, they are merged to form the completed model. The proposed modelling procedure is flexible in the sense that they allow the modelling of single and multiple surface cracks and can be extended to the case of SSHS Y-joints and RHS T-joints with modest additional modifications.

The convergence and validation study are performed to ensure the reliability of the mesh models generated by the mesh generator. Finite element analysis (FEA) of SCF for uncracked SSHS T-joints and SIF for cracked SSHS T-joints has been carried out extensively using the above proposed modelling technique. The numerical results showed that the proposed modelling procedure leads to accurate and more importantly, reasonably conservative results when comparing to the experimental measurements. Therefore, it can be concluded that the proposed methods can be used in practical applications for fatigue assessment of SSHS T-joints.

8.2.3 Finite Element Analysis on Crack Profiles, Weld Size and Contact Problems

The validated modelling technique is used to conduct the FEA on the crack profiles, weld size and contact problems that may influence the SIF distributions along the crack front due to the modelling difference. The investigation shows that the crack surface has significant effect on the SIF values at the deepest point. The crack surface cannot be represented by a straight line or a single-variable equation. It is a double-curved surface and a double-variable equation must be used to define the crack surface in both crack depth direction and crack length direction. A general crack surface equation is

suggested based on the full-scale test results and the numerical comparison. Both the SIF at the deepest point and along the crack front are compared between the models with actual measured crack surface and proposed crack surface. The good agreements show that the proposed crack surface equation is good enough for the SSHS T-joint fatigue analysis. In the investigation of the influence caused by the crack shape, the model with different l_d/l ratio is generated. The crack shape (l_d/l) affects largely on both the SIF values at the deepest point and the SIF distribution along the crack front. In practical application, HSS analysis can be carried out first to get the accurate crack shape modelling. In the investigation of the influence of the second crack on the development of the first crack, it is found that when the main crack and the second crack are very small, the model can be simplified to modelling the joint with one main crack, since the influence of the second crack is quite small at this condition. However, when both the main crack and the second crack are very deep and long, such as more than quarter of the brace circumference, the model with two cracks must be modelled.

The weld size (W) has significant influence on the SIF (K_e) results. When the weld size (W) increases, the SIF (K_e) value at the deepest point decreases. The shallower the crack is, the more significant the influence of the weld size (W) on the SIF (K_e) value. The weld size (W) has similar effect on K_I , but has little effect on K_{II} and K_{III} . Hence, in the modelling of weld size, AWS minimum values can be used since the model gives the most conservative SIF (K_e) results.

In the contact problem, model with cracks under three loads is investigated. The case under AX load shows that the contact surface definition on the crack surface causes little influence on the convergence of the results. When the crack is under comparison, contact surface must be defined since the SIF distribution is very different in the contact area. Even in the region out of the contact area, the SIF result is affected by the contact surface definition.

8.2.4 Fatigue Life Prediction

Once the accurate SIF can be obtained by the numerical analysis, a life prediction procedure for part-through crack (PTC) is proposed. In the procedure, the Paris Law is rearranged. By integrating the rearranged equation, the residual life, $N_r(a')$ which is the number of load cycles for the crack to penetrate from a curved depth of a' to a'_{final} can be estimated. The procedure is illustrated using the four full-scale test results. And its validity is demonstrated.

8.3 RECOMMENDATIONS FOR FUTURE RESEARCH WORK

1. Further studies of fatigue test for the SSHS T&Y-joints and other RHS joint geometry are highly recommended. In the current study, fatigue tests are carried out on four SSHS T-joints with the different geometrical parameters subjected to the combination of basic loads with different component ratios. The fatigue tests provide very important information for fatigue design but this is still not enough to clearly understand the fatigue performances of the SSHS T-joints under the various loadings. Furthermore, it is necessary to further investigate whether the conclusions drawn from the SSHS T-joints can be applied to other RHS joint geometry.
2. The proposed tailor-make generator, whereby the entire structure is divided into distinct zones to generate the finite element mesh of cracked SSHS T-joints, are found to be effective and efficient. The sub-zone CF containing the cracked zone sub-zone A can be easily applied and used for other types of RHS joints. This should be extended to Y, K and KK -joints in the immediate future.
3. In the current experimental studies, some phenomena are found during the testing, such as the crack branching and two crack problems. In the numerical analysis, two

crack problems are analyzed. However, the crack branching problem is omitted because of the limited information obtained from the experiments. As a preliminary analysis, the crack branching in SSHS joints may be caused by the material non-homogenous due to the fabrication of the tube. Hence, further testing should be conducted on the crack threshold in different directions of one SSHS tube.

4. A general crack surface equation is proposed in this study. The validity of the equation is demonstrated by the experimental results. However, its application on the SSHS T-joints and other RHS joints should further be confirmed.
5. In the crack shape modelling, bi-ellipse is used to represent the crack shape. The numerical analysis results are good and conservative comparing with experimental measurements. However, the parameter ratio l_d / l should further be investigated.

REFERENCES

1. ABAQUS, *User Manual*, version 6.4, (2004a). Hobbit, Karlsson & Sorensen Inc., USA.
2. ABAQUS, *Theory Manual*, version 6.4, (2004b). Hobbit, Karlsson & Sorensen Inc., USA.
3. Aliabadi, M.H. and Rooke, D.P., (1991), *Numerical Fracture Mechanics*, Computational Mechanic Publications, Southampton, UK.
4. American Petroleum Institute, (1993), "Recommended Practice for Planning, Designing and Constructing Fixed Offshore Platforms," *API-RP2A*, 20th Edition.
5. American Welding Society (AWS) (2000), "Structural Welding Code-Steel", *ANSI/AWS D1.1-2000*. American Welding Society, Inc, Miami.
6. American Welding Society (AWS) (1996), "ANSI/AWS D1.1-96 Structural Welding Code-Steel," Miami, USA.
7. ANSYS, (1996), Swanson Analysis System Inc., Houston, Texas.
8. Austin, J.A., (1994), "The Role of Corrosion Fatigue Crack Growth Mechanisms in Predicting the Fatigue Life of Offshore Tubular Joints," *Ph.D. Thesis*. University College London.
9. Bowness D. and Lee, M.M.K., (1995a), "A Finite Element Study of Stress Fields and Stress Intensity Factors in Tubular Joints," *Journal of Strain Analysis*, 30(2): 135-142.
10. Bowness, D. and Lee, M.M.K., (1996), "Stress Intensity Factor Solutions for Semi-Elliptical Weld-Toe Cracks in T-Butt Geometries," *Fatigue Fract. Engng Mater. Struct.*, Vol. 19, No. 6, pp.787-797.
11. Bowness, D., and Lee, M. M. K., (1995a), "The development of an accurate model for the fatigue assessment of doubly curved cracks in tubular joints", *International Journal of Fracture*, Vol. 73, pp.129-147.

12. Bowness, D. and Lee, M.M.K., (1995b), "The development of an accurate model for the fatigue assessment of doubly curved cracks in tubular joints," *International Journal of Fracture*, 73: pp.129-147.
13. Bowness, D., Lee, M.M.K., (1998), "Fatigue Crack Curvature under the Weld Toe in a Offshore Tubular Joint," *International Journal of Fatigue*, vol.20, No.6, pp.481-490.
14. Burdekin, F.M. and AL Laham, S., (1993). "Experimental and Finite Element Studies into the Ultimate Behaviour of Cracked and Uncracked Rectangular Hollow Section Fillet Welded K-joints with Gap," *Tubular Structures V: Proceedings of the 5th International Symposium on Tubular Structures*, Nottingham, UK, pp.691-702.
15. Burdekin, F.M., Chu, W.H., Chan, W.T.W. and Manteghi, S., (1986), "Fracture Mechanics Analysis of Fatigue Crack Propagation in Tubular Joints," *International Conference on Fatigue and Crack Growth in Offshore Structures*, IMechE, London, UK, C133/86.
16. Burdekin, F.M., Garwood, S.J. and Milne, I., (1988), "The Basis for the Technical Revisions to the Fracture Clauses of PD 6493," *TWI International Conference — Weld Failures*, Paper 37, pp.21-24, London.
17. Cao, J.J., Yang, G.J., Packer, J.A., (1997), "FE mesh Generation for Circular Tubular Joints with or without Cracks," *Proceedings of the Seventh International Offshore and Polar Engineering Conference*, Honolulu, HI, Vol. IV, pp.98-105.
18. Cao, J.J., Yang, G.J., Packer, J.A., Burdekin, F.M., (1998), "Crack modeling in FE analysis of circular tubular joints," *Engineering Fracture Mechanics*, pp.537-553.
19. Chiew, S.P., Lie S.T., Lee, C.K. and Huang, Z.W., (2004), "Fatigue performance of cracked tubular T-joints under combined loads - part I - experimental", *Journal of Structural Engineering, ASCE*, 130(4), pp.562-571.
20. Chiew, S.P., Lie, S.T., Lee, C.K. and Huang, Z.W., (2001). "Stress Intensity Factors of Through-thickness and Surface Cracks in Tubular Y-joints", *Proc. of the Eleventh International Offshore and Polar Engineering Conference (ISOPE-2001)*, Stavanger, Norway, Paper No: 01-JSC-284.

21. Chiew, S.P., Lie, S.T., Lee, C.K., and Huang, Z.W., (2001), "Stress intensity factors for a surface crack in a tubular T-joint", *International Journal of Pressure Vessels and Piping*, Vol. 78, pp.677-685.
22. Chong Rhee, H., Han, S., and Gipson, G. S., (1991), "Reliability of Solution Method and Empirical Formulas of Stress Intensity Factors for Weld Toe Cracks of Tubular Joints," *Proc. 10th Offshore Mechanics and Arctic Engineering Conference*, ASME, Vol. 3-B, pp.441-452.
23. Department of Energy (DEn), (1984), "Offshore Installations: Guidance on Design and Construction," HMSO, UK.
24. Department of Energy (DEn), (1990), "Offshore Installations: Guidance on Design and Construction," Department of Energy, London, UK.
25. Department of Energy (DEn), (1993), "Background to New Fatigue Design Guidance for Steel Joints in Offshore Structures," Internal Report, Department of Energy, London, UK.
26. Dijkstra, O.D. and Noordhoek, (1985). "The Effect of Grinding and a Special Weld Profile on the Fatigue Behaviour of Large-scale Tubular Joint", *Proc. 17th Int. Conf. on Offshore Tech. Conf.* Texas, USA, OTC 4866, pp.229-235.
27. Dover W.D. and Connolly, (1986), "Fatigue Fracture Mechanics Assessment of Tubular Welded Y and K-Joints," *International Conference on Fatigue and Crack Growth in Offshore Structures*, IMechE, London, UK, C141/86.
28. Dover, W. D., Dharmavasan, S., Brennan, F. P., and Marsh, K.J., (1995), "Fatigue Crack Growth in Offshore Structures," *Engineering Materials Advisory Services (EMAS) Ltd.*, Chameleon Press, London, UK.
29. Dover, W.D. and Dharmavasan, S., (1982), "Fatigue Fracture Mechanics Analysis of T and Y joints," *Proc. Int. Offshore Technology Conference*, Houston, paper OTC 4404.
30. Dover, W.D. and Monahan, C.C., (1994), "The measurement of surface breaking cracks by the electrical systems ACPD/ACFM", *Fatigue & Fracture of Engineering Materials & Structures*, Vol. 17, No. 12, pp.1485-1492.
31. Du, Z.Z. and Hancock J.W., (1987), "Stress Intensity Factors of Curved Semi-Elliptical Cracks in a Tubular Welded Joint Using Line Springs and 3D Finite Elements," *International Journal of Pressure Vessels and Piping*, pp.111-247.

-
32. Dutta, D., (1996), "Parameters influencing the stress concentration factors in joints in offshore structures", *Fatigue in Offshore Structures*, Vol.1, A.A. Balkema, Rotterdam, pp.77-128.
 33. EC3, (1992), "Design of Steel Structures - Part 1.1: General Rules and Rules for Buildings," ENV 1993-1-1, *Eurocode 3*, European Committee for Standardisation (CEN).
 34. EN 10210-2, (1997), "Hot finished structural hollow sections of non-alloy and fine grain structural steels. Part 2, Tolerances, dimensions and sectional properties", *British Standards Institution. BSI*.
 35. Erdogan, F. and Sih, G.C., (1963). "On the Crack Extension in Plates under Plane Loading and Transverse Shear", *J. Bas Engng*, Vol.85, pp.519-525.
 36. Etube, L.S., Brennan, F.P. and Dover, W.D., (2000), "A New Method for Predicting Stress Intensity Factors in Cracked Welded Tubular Joints," *International Journal of Fatigue*, 22 pp. 447-456
 37. Frater. G.S. and Packer, J.A., (1993), "Strain concentration factors in rectangular hollow section truss gap K-connections – An experimental study", *J. Construct. Steel Research*, Vol. 24, pp.77-104.
 38. Fu, B., Haswell, J.V. and Bettess, P., (1993), "Weld magnification Factors for Semi-Elliptical Surface Cracks in Fillet Welded T-butt Joint Models," *International Journal of Fracture*, 63, pp.155-171.
 39. Gulati, K.C., Wang, W.J., and Kan, D.K.Y., (1982), "An Analytical Study of Stress Concentration Effects in Multibrace Joints under Combined Loading," *Proceedings 14th Annual Offshore Technology Conference*, OTC 4407, Houston, TX, pp.337-355.
 40. Haswell, J., (1991), "Simple models for Predicting Stress intensity Factors for Tubular Joints," *Fatigue Fract. Engng. Mater. Struct.*, Vol.14, n5. pp.499-513.
 41. Haswell, J., Hopkins, P., (1991), "A Review of Fracture mechanics Models of Tubular Joints," *Fatigue Fract. Engng. Mater. Struct.*, Vol.14, n5 pp.483-497.
 42. Herion, S., Mang, F., Puthli, R., (1996), "Parametric Study on Multiplanar K-joints with Gap Made of Circle Hollow Sections by Means of the Finite Element method," *Proceeding of the Sixth International Offshore and polar Engineering Conference*, Los Angeles, CA, Vol.IV, pp.68-73.
-

-
43. Huang Z.W., (2002). "Stress Intensity Factor of Steel Tubular T and Y-joints under Combined Loads," *PhD Thesis*, Nanyang Technological University, Singapore.
 44. Huang, X., Du, Z.Z. and Hancock, J.W., (1988), "A Finite Element Evaluation of the Stress Intensity Factors of Surface Cracks in a Tubular T Joint," *Offshore Technology Conference*, OTC 5165.
 45. International Institute of Welding (IIW), (1999), "Recommended Fatigue Design Procedure for Hollow Section Joints: Part 1-Hot Spot Stress Method for Nodal Joints," IIW Doc. XV-1035-99, XV-E-99-251, XIII-1804-99, Lisbon, Portugal, July.
 46. Irwin G.R., (1957), "Analysis of Stresses and Strains Near the End of a Crack Traversing a Plate," Transactions, ASME, *Journal of Applied Mechanics*, Vol. 24.
 47. Lee, C.K., Lie, S.T., and Wong, S.M., (1999), "Model and Mesh Generation of Cracked Tubular Joints," *Proceedings of the European Conference on Computational Mechanics*, Munich, Germany, pp.182-183.
 48. Lee, M.M.K. and Bowness, D., (2001), "Prediction of Stress Intensity Factors in Semi-elliptical Weld Toe Cracks in Offshore Tubular Joints," *Proceedings of the 9th International Symposium and Euroconference on Tubular Structures*, Dusseldorf, Germany, pp.299-308.
 49. Lie S.T., Chiew, S.P., Lee C.K. and Huang, Z.W., (2004), "Fatigue performance of cracked tubular T-joints under combined loads - Part II - Numerical", *Journal of Structural Engineering, ASCE*, 130(4), pp.572-581.
 50. Lie, S.T., Chiew, S.P., Lee, C.K., Wong, S.M. and Huang, Z.W., (2000), "Modelling Arbitrary Through-thickness Crack in a Tubular T-joint," *Proceedings of the 10th International Offshore and Polar Engineering Conference (ISOPE 2000)*, Seattle, USA, Vol. IV, pp.53-58.
 51. Lie, S.T., Lee, C. K. and Wong S.M., (2003), "Model and mesh generation of cracked tubular Y-joints", *Engineering Fracture Mechanics*, Vol.70, pp.161-184.

52. Ling, W., (1993), "Some Aspects of the Prediction of Fatigue Crack Paths", *Mixed-Model Fatigue and Fracture*, Rossmanith, H.P. and Miller, K.J., pp.201-215
53. Maddox, S.J., (1975), "An Analysis of Fatigue Cracks in Fillet Welded Joints," *International Journal of Fracture*, II, pp.221-243.
54. Mashiri, F.R., Zhao, X.L. and Grundy, P., (2002), "Fatigue Tests and Design of Thin Cold-formed SHS-to-SHS T-Connections under In-Plane Bending", *Journal of Structural Engineering, ASCE*, 128(1), pp.22-31.
55. Mashiri, F.R., Zhao, X.L. and Grundy, P., (2004a), "Stress Concentration Factors and Fatigue Failure of Welded T-Connections in Thin Cold-Formed Circular Hollow Sections under In-Plane Bending", *International Journal of Structural Stability and Dynamics*, 4(3), pp.403-422.
56. Mashiri, F.R., Zhao, X.L. and Grundy, P., (2004b), "Stress Concentration Factors and Fatigue Behaviour of Welded Thin-Walled CHS-SHS T-Joints under In-Plane Bending", *Engineering Structures*, 26(13), pp.1861-1875.
57. Tong, L.W., Zhao, X.L., Mashiri, F.R., Grundy, P. and Zheng, H.Z., (2006), "Fatigue Behaviour of Welded Thin-Walled T-Joints between Circular and Square Hollow Sections", *International Journal of Steel Structures*, accepted for publication Vol.6, No.1.
58. Monahan, C.C., (1994), "Early Fatigue Crack Growth in Offshore Structure," *PhD Thesis*, University College London.
59. Myers, P.T., (1998), "Corrosion Fatigue and Fracture Mechanics of High Strength Jack Up Steels," *Ph.D. Thesis*, University College London.
60. Naess, A.A., (1985), "Fatigue Handbook-Offshore Steel Structure," Tapir, Norge.
61. Newman, J.C. and Raju, I.S., (1981), "An Empirical Stress Intensity Factors Equation for the Surface crack," *Engineering Fracture Mechanics*, 15(1-2), pp. 185-192
62. Nui, X., Glinka, G., (1989), "Stress Intensity Factors for Semi-Elliptical Surface cracks in Welded Joints," *International Journal of Fracture*, 40, pp.255-270.
63. Paris, P.C. and Erdogan, F., (1963), "A Critical Analysis of Crack Propagation laws," *Journal of Basic Engineering*, 85(4), 528-534.

-
64. PATRAN User Manual, (1993), PDA Engineering, USA
 65. Pook, K.P., (2000), *Linear Elastic Fracture Mechanics for Engineers. Theory and Applications*, WIT Press: Southampton.
 66. PRETUBE User's Manual Veritas Sesam System, (1991), Det Norske Veritas (DNV) Industry Ltd., Hovik, Norway,
 67. Puthli, R.S., de Kong, C.H.M., Wardenier, J. and Dutta, D., (1986). "A Study on Strain Concentration Factors of Square Hollow Section X-joints with Brace in Tension", *Stevin report 6-86-7, TNO-IBBC report BI-86-63/63.5.3820*, Delft, the Netherlands.
 68. Puthli, R.S., Wardenier, J., de Koning, C.H.M., van Wingerde, A.M. and van Dooren, F.J. (1988), "Numerical and experimental determination of strain (stress) concentration factors of welded joints between square hollow sections", *Heron*, the Netherlands, Vol.33, No.2, pp.1-50.
 69. Qian R.J., (1996), "Applying Strain Energy Density Factor Theory to Propagation Estimating of Surface Crack in Tubular T-joints," *Engineering Fracture Mechanics*, 53(6): pp.849-858.
 70. Rhee, H.C. and Salama, M.M., (1987), "Mixed-Mode Stress Intensity Factor Solutions of A Warped Surface Flaw by Three-Dimensional Finite Element Analysis", *Engineering Fracture Mechanics*, Vol.28, No.2, pp.203-209.
 71. Ritchie, D., Voermans, C.W.M. and Vollan, K., (1986), "Stress Intensity Factors in an Offshore Tubular Joint Test Specimen", *Proc. Of the 4th Int. Conf. on Numerical Methods in Fracture Mechanics*, pp.715-726.
 72. Rollason, E.C., (1973), *Metallurgy for Engineers*, 4th edition, Edward Arnold (Publishers) LTD.
 73. Salama, M.M., Toyoda, M., Liu, S., Dos Santos, J.F. and Williams, J., (1993), (eds). *Proc. of the 12th Int. Conf. on Offshore Mechanics and Arctic Engineering*, American Society of Mechanical Engineers: New York, Vol.IIIB.
 74. Shao Y.B., (2004) "Fatigue Behaviour of Uniplanar CHS Gap K-joints under Axial and In-plane Bending Loads" *PhD Thesis*, Nanyang Technological University, Singapore.

-
75. Shih, C.F. and Asaro, R.J., (1988), "Elastic-Plastic Analysis of Cracks on Bimaterial Interfaces: Part I—Small Scale Yielding," *Journal of Applied Mechanics*, pp.299-316.
 76. Soh, A.K. and Soh, C.K., (1990). "A Parametric Stress Analysis of T/Y and K Square-to-Square Tubular Joint", *Journal of Constructional Steel Research*, Vol.15, pp.173-190.
 77. Soh, A.K. and Soh, C.K., (1992). "Stress Concentrations in T/Y and K Square-to-Square and Square-to-Round Tubular Joints", *Journal of Offshore Mechanics and Arctic Engineering*, 114(3), pp.220-230.
 78. Stig, B., Oddvar, I.E. and Peter, J.T., (1994), "Fatigue Strength of Tubular Joint: Some Unresolved Problems", *Proceedings of International Conference on Offshore Mechanics and Arctic Engineering*, Vol.3, USA.
 79. Strommen, E.N. and Larsen, P.K., (1994), "Fatigue tests on welded RHS lattice girder joint", *J. Construct. Steel Research*, Vol. 31, pp.103-114.
 80. Tada, H., Paris, P.C. and Irwin, G.R., (1973), "The Stress Analysis of Cracks Handbook," Del Research Corporation Hellertown, Pennsylvania.
 81. Technical Software Consultant Ltd. (TSC), (1991), *ACFM Crack Microgauge – Model U10*, Milton Keynes, UK.
 82. Technical Software Consultant Ltd. (TSC), (1998), *FLAIR User's Manual – Software Version 1.9*, Milton Keynes, UK.
 83. Thorpe, T.W., (1986), "A Simple Model of Fatigue Crack Growth in Welded Joints," Offshore Technology Report, OTH 86 255.
 84. UEG, (1985), "Design of Tubular Joints for Offshore Structures," Underwater Engineering Group, Vol. 1, Publication UR33, UK.
 85. van Delft, D.R.V., Dijkstra, O.D. and Snijder, H.H., (1986), "The calculation of Fatigue Crack Growth in Welded Tubular Joints Using Fracture Mechanics," *Offshore Technology Conference*, OTC 5352.
 86. van Wingerde, A.M., (1992), "Fatigue behavior of T and X joints made of square hollow sections", *Heron*, the Netherlands, Vol.37, No.2, pp.1-182.

87. van Wingerde, A.M., Packer, J.A. and Wardenier, J., (1995), "Criteria for the Fatigue Assessment of Hollow Structural Section Connections," *Journal of Constructional Steel Research*, Elsevier, pp.71-115.
88. van Wingerde, A.M., Packer, J.A. and Wardenier, J., (1996), "New Guidelines for Fatigue Design of HSS Connections", *Journal of Structural Engineering*, Vol. 122 No.2 ASCE, pp.125-132.
89. van Wingerde, A.M., van Delft, D.R.V., Wardenier, J. and Packer, J.A., (1997), "Scale Effects on the Fatigue Behavior of Tubular Structure," *IIW International Conference on performance of Dynamically Loaded Welded Structures*, July San Francisco, USA.
90. van Wingerde, A.M., Wardenier, J. and Packer, J.A., (1998), "Commentary on the Design Specification for Fatigue Design of Hollow Section Joints," 8th *International Symposium on the Tubular Structures*, Singapore.
91. Vosikovsky, O., Bell, R., Burns, P.J. and Mohauph, U.H., (1985), "Fracture mechanics assessment of fatigue life of welded plate T-joints, including thickness effect", *Boss Conference*, Amsterdam.
92. Wong S.M., (2001). "Model and Mesh Generation of Cracked Y-tubular Joints for SIF and SCF Studies," *MEng Thesis*, Nanyang Technological University, Singapore.
93. Yang, G.J., (1996). "Effect of mixed mode fracture on ultimate strength of cracked tubular joints," *Ph.D. thesis*, Department of Civil and Structural Engineering, UMIST, UK.
94. Zhao, X.L, Herion, S., Packer, J.A., Puthli, R., Sedlacek, G., Wardenier, J., Weynand, K., van Wingerde, A. and Yeomans, N., (2000). "Design Guide for Circular and Rectangular Hollow Section Joints under Fatigue Loading". Verlag, TÜV Rheiniand GmbH, Cologne.
95. Zhao, X.L., Grundy, P., Munch, C. and Mashiri, F.R., (2001), "Fatigue crack initiation and propagation in thin SHS-to-SHS T-joints", *Tubular Structures IX*, Puthli and Herion (eds), Swets and Zeitlinger, Lisse, ISBN 90 58059 1910, pp. 269-276.

PUBLICATIONS

JOURNAL PAPERS

1. Chiew, S.P., Lee, C.K., Lie, S.T. and Ji, H.L., (2006), "Fatigue Behaviors of Square-to-Square Hollow Section T-joint with Corner Crack I: Experimental Studies", (*Accepted by International Journal of Engineering Fracture Mechanics*).
2. Lee, C.K., Chiew, S.P., Lie, S.T. and Ji, H.L., (2006), "Fatigue Behaviors of Square-to-Square Hollow Section T-joint with Corner Crack II: Numerical Modeling", (*Accepted by International Journal of Engineering Fracture Mechanics*).

CONFERENCE PAPERS

1. Chiew, S.P., Lie, S.T., Lee, C.K. and Ji, H.L., (2002), "Stress Analysis of Square-to-Square Tubular T-joint under Combined Loads", *Proc. 7th International Conference on Steel and Space Structures*, Singapore, pp.199-206.
2. Chiew, S.P., Lie, S.T., Lee, C.K. and Ji, H.L., (2005), "Large-scale Fatigue Tests on Square Tubular T-joints", *Proc. Of the First International Conference on Advances in Experimental Structural Engineering, AESE-2005*, Nagoya, Japan, pp.557-564.
3. Chiew, S.P., Lee, C.K., Lie, S.T. and Ji, H.L., (2005), "Numerical Modeling of Square Tubular T-Joints with Surface Crack at Corner", *Proc. Of the Fourth International Conference on Advances in Steel Structures, ICASS-05*, Shanghai, China, Vol 1, pp.1159-1164.
4. Chiew, S.P., Lie, S.T., Lee, C.K. and Ji, H.L., (2003), "Fatigue Analysis of Square-to-Square Tubular T-joint with Surface Crack", *Proc. Of the 10th International Symposium on Tubular Structures, ISTS 10*, Madrid, Spain, pp. 375-382.

5. Chiew, S.P., Lie, S.T., Lee, C.K. and Ji, H.L., (2006), "Crack Surface under Weld Toe in Square Hollow Section T-joints", *Proc. Of the 11th International Symposium on Tubular Structures*, ISTS 11, Quebec, Canada, (*Accepted for publication*).

APPENDIX A
AWS(2000) WELDING SPECIFICATIONS

Notes:

1. The dihedral angle is defined as Ψ .
2. " t_b " is the thickness of brace.
3. ϕ is the joint included angle.
4. ω is the end preparation angle.
5. L is the size of fillet.
6. R is the root opening (joint fit-up).
7. W is the backup weld width.

APPENDIX B
EN10210 (1997) DIMENSION MEASURING OF SQUARE
OR RECTANGULAR HOLLOW DECTION

* This dimension is a maximum when measuring B or H and a minimum when measuring T

Figure 1. Limiting cross-sectional positions for measuring the dimensions B , H and T for square or rectangular hollow sections

Figure 2. Outside corner profile of square or rectangular hollow sections

Note: C_1/C_2 :- Length of corner region;

R : Radio of corner.