Model and mesh generation of partially overlapped circular hollow section k-joints for fatigue studies

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**MODEL AND MESH GENERATION OF
PARTIALLY OVERLAPPED CIRCULAR HOLLOW
SECTION K-JOINTS FOR FATIGUE STUDIES**

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I would like to dedicate this thesis to my mother.

SUMMARY

In this study, a novel and consistent geometrical model and mesh generation technique is proposed for partially overlapped CHS K-joints with and without crack. For the mesh without crack, the geometrical model of welding details is developed both on the chord and brace sides by a new approach such that the cut off requirements by AWS (1996) and API (2000) can be satisfied. Welding parameters are verified and adjusted by the measurement of welding thickness on small and full scale specimens. Furthermore, a general algorithm is developed for the determination of the theoretical and actual intersection points between the braces and the chord, which is a distinctive feature of partially overlapped CHS K-joints. For the mesh with crack, a general model for the inclined crack surface and an unsymmetrical crack front are proposed.

Based on the developed geometrical model, a new mesh modelling method is proposed for partially overlapped CHS K-joints. The mesh generator is constructed step-by-step, therefore it is able to produce several kinds of meshes such as surface meshes, solid meshes, meshes with or without welding and crack details. At each step, a particular mesh can be exported depending on the complexity of the particular fatigue problem under consideration. The application range of the mesh generator is extended for special cases of identical chord and braces dimensions as well as large overlapped percentage, which are not commonly covered by other commercial software packages. Most importantly, it is able to generate a solid mesh with welding details and surface crack of any length and locates at either sides of the joint intersection.

In the experimental program, two full-scale partially overlapped CHS K-joints were tested under cyclic combined loads in order to gather more information about the fatigue performance of the joints, as well as to evaluate the surface crack model proposed. During the tests, the crack initiation and propagation were monitored. The weld profile, shape and surface of the crack of the two full-scale specimens were measured. It is noted that the fatigue test with a certain combination of loading was able to cause the crack to occur along the brace side of the weld. It is seen that the fatigue life of the two specimens were relatively short as the crack penetrated into the thickness of the wall of the CHS quicker than the propagation speed along the

intersection The fatigue life corresponding to the HSS range of each specimen is used to validate the CIDECT S-N curve (Zhao et al. 2000).

The mesh generator is used to generate FE models with exact geometry of the two tested partially overlapped CHS K-joints. The numerical SCF and SIF values obtained from these models compare well with the respective results obtained from the two tested specimens. In addition, several FE models for the assessment of SCF and SIF values are presented for comparison and consideration. It is recommended that in case actual measurement is not available, the solid mesh with welding details, mesh SOLID_1W, is used for SCF prediction and the mesh SOLID_CR with the surface crack angle $\omega = 0$ is used for SIF prediction of partially overlapped CHS K-joints. Furthermore, residual life prediction procedure for partially overlapped CHS K-joints is proposed. It is concluded that the numerical models with crack together with the residual life prediction procedure can be used to predict the remaining life of a partially overlapped CHS K-joints with existed crack, provided that a safety factor greater than 1.4 is used.

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LIST OF SYMBOLS

a'_{initial}	initial crack depth
a'_{final}	final crack depth
d	Distance between the origins of two coordinate systems $OXYZ$ and $O^{(2)}X^{(2)}Y^{(2)}Z^{(2)}$
e	Distance between the origins of two coordinate systems $O^{(3)}X^{(3)}Y^{(3)}Z^{(3)}$ and $O^{(3')}X^{(3')}Y^{(3')}Z^{(3')}$
$FoS_{\text{outer}}, Fos_{\text{inner}}$	Scale factors of outer and inner thickness
$FOS_N(a')$	Factor of safety
g	Length of the overlap between overlap and through brace
h	Diameter of the overlap brace projected on chord axis
K	Stress intensity factor
k_1	Effective surface contact thickness factor
k_2, k_3	Outer and inner modification factors for weld model on the chord side
k'_2, k'_3	Outer and inner modification factors for weld model on the brace side
k_{AWS}	Minimum theoretical weld thickness factor specified by AWS
k_{TW}	Modification factor of modelled weld thickness
n_2, n_3	Weld thickness modelling constants
$N_{\text{act}}(a')$	Tested load cycles recorded by ACPD
$N_r(a')$	Predicted load cycles
q	Eccentricity of the intersection point between overlap and through brace to the chord axis
p	Percentage of overlap
R_0	Radius of chord
r	Radius of brace
R_1	Radius of through brace
R_2	Radius of brace
T_{AWS}	Minimum specification of AWS for weld thickness

List of Symbols

T_w	Actual/modelled weld thickness
T_1	Original contact thickness
T_2, T_3	Modified outer and inner thickness
u, v	Local planar co-ordinate system used to define circle
u', v'	Normalized planar space co-ordinate system
$OXYZ$	Global coordinate system to define chord and through brace intersection curve
$O^{(2)}X^{(2)}Y^{(2)}Z^{(2)}$	Local coordinate system to define chord and overlap brace intersection curve
$O^{(3')}X^{(3')}Y^{(3')}Z^{(3')}$	Local coordinate system to define through brace
$O^{(3)}X^{(3)}Y^{(3)}Z^{(3)}$	Local coordinate system to define overlap and through brace intersection curve
α	Polar angle defined in u-v coordinate system
β_a	Angle between a normal at intersection and horizontal Z-axis in Y-Z plane
β	Brace-to-chord radius ratio
γ	Dihedral angle
ϕ	Corresponding angle defined in Y-Z plane
θ	Intersecting angle between overlap and through brace
θ_1	Intersecting angle between through brace and chord
θ_2	Intersecting angle between overlap brace and chord
θ_s	Minimum intersecting angle assumed for weld modelling
ψ	Local polar angle defined on chord circle
ω	Crack surface angle

CHAPTER 1

INTRODUCTION

1.1 TUBULAR JOINTS IN OFFSHORE STEEL STRUCTURES

Jacket structures (Fig.1.1) are a reliable and robust solution for most offshore environments in water depths from 10m up to 300m (Halliburton 2004). These structures comprise steel tubular frameworks of braces and chords fixed to the seabed by piles. The reason for which tubular structures are utilized is their low drag coefficient, high buoyancy and high strength-to-weight ratio. Besides, their high torsional rigidity and great buckling capacity makes ideal application in offshore structures. Other than the tubular members, tubular joints are important since they promote the connection between members. Inspection and cyclic tests on tubular frameworks have shown that tubular joints are the very positions where failure initiates. Therefore, in order to avoid catastrophic consequences, it is necessary to ensure that failure will not occur at the joint.

The design of tubular joint in particular and jacket structure in general is controlled by criteria depending on the location of the jacket structure. For example, the design of offshore structures in the Gulf of Mexico is governed by criteria on storm and seismic loading. While in the North Sea, where concentrates the greatest number of large steel jacket structures in the world, fatigue is the main requirement (Billington et al. 1987). It is because the weather in the North Sea is particularly severe. Waves corresponding to a hundred-year return period could have heights up to 31 m and period of between 15 and 20 seconds. Towards this end, the HSE (Health & Safety Executive, UK) statistic data of the North Sea steel structures have proved that fatigue damage is the most frequent single cause of repairs, representing some 25% of all repairs to the steel platform (Stacey and Sharp 1997). Therefore, the understanding of the fatigue behaviour of tubular structures becomes essential. However this is also a challenging task since there is great uncertainty about the loads to which a tubular joint subjected to, and the extreme difficulty when a tubular joint with welding and crack details is to be numerically modelled.

To date, there are two methods for assessing the fatigue performance of tubular joints currently in use in the offshore industry. The first method is based on the use of hot spot stress (HSS) in comparison with a set of fatigue life curves (S-N curves) obtained from fatigue test data. The method is therefore named as the HSS method and is used almost exclusively in practical design of tubular joints. However this approach relies on the accurate prediction of the HSS at the joint intersection and the construction of the S-N curves is limited to the availability of tubular joints tested. Furthermore, the S-N curves only provide the total fatigue life of a tubular joint which includes the crack initiation and propagation phases. The use of the HSS method is therefore not possible whenever a crack is detected in the service life of a tubular joint. It is because the existence of a crack suggests that the crack has entered the propagation phase which accounts for 90% of a joint's life (Haswell and Hopkins 1991). Towards this end, the fracture mechanics approach can be applied for the investigation of the crack growth behaviour. The fracture mechanics method depends on the calculation of the stress intensity factor (SIF) along the crack front. For the solution of SIF, experimental as well as numerical analysis has been carried out and the fracture mechanics method is specified in design codes for tubular joints, such as BS 7910 (1999) and API RP 579 (2000) to assess the fitness for service criteria. However due to the complex geometry and loading conditions inherent in the tubular joints, the reasonable determination of SIF for a particular joint type is still an evolving technology.

1.2 JOINT CLASSIFICATION

Tubular joints appear in great variety of shapes. Generally, based on their geometrical properties, tubular joints can be classified as simple welded joints and complex welded joints. An illustration of some joint shapes is given in Fig. 1.2. In practice, simple gap joints are the most popular connection details in the offshore industry. It is because of the readily available of design equations to assess the joint capacity. It is also because of the ease of fabrication, non-destructive testing and inspection that can be apply to them. However, due to the gap between braces, additional moments on the chord member should be carefully considered. BOMEL (1992), in an industry project investigating the strengths of tubular frames, found that the gap K-joints exhibited a typical brittle response and shed a large proportion of the load.

In contrast, partially overlapped CHS K-joint had a high residual capacity as a result of their optimized load transfer pattern. This advantage, however, is somehow offset by the higher fabrication costs due to the complex end profile of the overlap brace and the difficulty of the inspection of the hidden weld. Recently, Tizani et al. (1996) reported a case study comparing the costs of three K-joints design options. After due consideration of material and fabrication costs, it was concluded that the specification of an overlap joint was the cheapest solution, with the fabrication cost actually being significantly cheaper than that for the alternative canned gap joint. In addition, partially overlapped CHS K-joint are still specified particularly for joints of large brace/chord ratios or when brace members meet at the chord axis to minimize the footprint length, or in cases where the required thickness of a joint can would exceed fabrication limit. The effectiveness of partially overlapped CHS K-joints is discussed in the next Section.

1.3 UNDERSTANDING OF THE EFFECTIVENESS OF PARTIALLY OVERLAPPED CHS K-JOINTS

Consider a general K-joint with its configuration and parameter ratios plotted in Fig. 1.3. The gap length between two braces can be calculated as:

$$g = (e + R_0) \left(\frac{1}{\tan \theta_1} + \frac{1}{\tan \theta_2} \right) - \frac{R_1}{\sin \theta_1} - \frac{R_2}{\sin \theta_2} \quad (1.1)$$

when $g > 0$, the joint is considered gap K-joint, otherwise it is a partially overlapped CHS K-joint.

In Eqn. (1.1), e is defined as the eccentricity the two braces made to the chord axis relatively. The existence of eccentricity in a K-joint induces moments in the chord, braces and in the joint itself. In EuroCode3 (2005), it is required that e should be limited in a certain range, which can be written as:

$$-0.55d_0 \leq e \leq 0.25d_0,$$

$$\text{or} \quad -1.1R_0 \leq e \leq 0.5R_0. \quad (1.2)$$

When the condition described in Eqn. (1.2) is satisfied, the respective moment in braces and tension chord might be neglected. However, a compression chord, which is normal in space truss structures, should take account of this moment. Therefore, the magnitude of eccentricity and the percentage of overlap or gap are essential factors to

assess the effectiveness of a K-joint.

If the parameter ζ is defined as the ratio g/R_0 and the parameter k is defined as:

$$k = \frac{e + R_0}{R_0}, \quad (1.3)$$

Eqn. (1.1) can then be rearranged as:

$$\zeta = k \left(\frac{1}{\tan \theta_1} + \frac{1}{\tan \theta_2} \right) - \frac{\beta_1}{\sin \theta_1} - \frac{\beta_2}{\sin \theta_2}. \quad (1.4)$$

The range of k can be inferred from Eqn. (1.2) as:

$$-0.1 \leq k \leq 1.5. \quad (1.5)$$

Non-eccentric K-joint can be obtained with $e = 0$, that equals $k = 1.0$. For other parameters, the EuroCode3 (2005) suggest their values as: $30^\circ \leq \theta_1, \theta_2 \leq 90^\circ$ and $0.2 \leq \beta_1, \beta_2 \leq 1.0$.

In practice, it is not uncommon for a K-joint to have symmetric configuration, that is $\theta_1 = \theta_2$ and $\beta_1 = \beta_2$. The ratio of gap length over chord radius therefore can be written as:

$$\zeta = \frac{2k}{\tan \theta} - \frac{2\beta}{\sin \theta} \quad (1.6)$$

For symmetric joints, the value of ζ calculated by Eqn. (1.6) is shown in Fig. 1.4(a). The range of ζ is the space bounded by two surfaces plotted using the lower and upper limits of k . Because the joint can be considered overlapped when $\zeta < 0$, the values of ζ is compared with the plane $\zeta = 0$ as shown in Fig. 1.4(b). From the figure, it is observed that for certain configuration of joints, especially for joints with high β ratio and large intersecting angle θ , overlapping is inevitable. Furthermore, in the range of β from 0.2 to 1.0 and θ from 30° to 90° , the percentage of over of unavoidable partially overlapped joints can count up to approximately 40%. For other unsymmetric configurations of K-joints, similar graph can be plotted and approximately similar result is expected.

If there is a strict requirement to avoid the eccentricity, the geometrical parameters for a symmetric joint can be checked against the condition of gap or overlapped in Fig. 1.5. Again, it is emphasized that for K-joints with high β ratio and large intersection angle θ , the overlapped joint helps reduce the eccentricity. For other un-symmetric K-

joints, the condition of gap or overlapped can be verified by fixing β_1 and θ_1 , then varying β_2 and θ_2 , or vice versa. For example, as shown in Fig. 1.6, if the values $\theta_1 = 60^\circ$ and $\beta_1 = 0.8$ are fixed, then with any β_2 less than 0.8 and any variable θ_2 , a partially overlapped CHS K-joint must be formed if non-eccentric joint is desired.

In summary, it can be concluded that in many situations, the use of partially overlapped K-joints is unavoidable even if the condition of non-eccentricity is not strictly required. For other cases, when non-eccentric joints are desired, overlapping is essential.

1.4 OBJECTIVES AND SCOPE OF THE RESEARCH

Although partially overlapped CHS K-joints exist in many offshore platforms as key structural elements, only very limited research works published on their fatigue behaviours and the modelling procedures for this form of structural connection. The current research project focuses on the modelling and mesh generation of cracked partially overlapped CHS K-joints for the evaluation of SCF and SIF. The main objectives of the current research are listed below:

- To develop a geometrical model of a general partially overlapped CHS K-joint, which includes the welding and surface crack details, as well as the intersection points between various weld paths;
- To develop an automatic mesh generator. The mesh will be able to generate the mesh of partially overlapped CHS K-joints with and without a surface crack. In case a crack is included, the mesh generator will allow for arbitrary crack length and crack locations;
- To investigate experimentally the fatigue behaviour of the partially overlapped CHS K-joints through fatigue tests conducted on two full-scaled specimens;
- To investigate numerically the fatigue behaviour of the partially overlapped CHS K-joints through FE analyses. Several uncracked and cracked numerical models will be suggested to model joints subjected to basic and combined loadings. From the results obtained, a practical numerical model for the assessments of SCF and SIF values for partially overlapped K-joints will be suggested;

- To propose a prediction procedure and a safety factor for the estimation of the residual life of partially overlapped CHS K-joints with surface crack.

The main originality of the work includes the followings:

- The development of a realistic geometrical model for partially overlapped CHS K-joints with welding and crack details;
- The development of a flexible mesh generation procedure which is able to accommodate the proposed geometrical model;
- The experimental investigation on the crack initiation and propagation for full-scaled partially overlapped CHS K-joints;
- The proposing a practical numerical model for the assessments of SCF, SIF and the residual life of partially overlapped CHS K-joints.

1.5 ORGANIZATION OF THE THESIS

The contents of this thesis are arranged as follows:

- Chapter 2 covers the literature review of the topic studied in this thesis. Past research works are reviewed and the related theories on SCF, SIF are recalled.
- Chapter 3 presents the geometrical modelling of a typical partially overlapped CHS K-joint. Key parameters defining a realistic weld path are studied and the actual intersection points in the common area are determined.
- Chapter 4 describes the mesh generation procedure which extrudes a surface mesh into a solid mesh. The procedure employed to incorporate the welding and crack details to form a solid mesh with and without crack will also be presented.
- Chapter 5 presents the experimental results obtained from the fatigue tests conducted on two full-scaled partially overlapped CHS K-joints.
- Chapter 6 focuses on the verification of the developed FE models with and without crack. FE analyses of several numerical models for the determination of SCF and SIF values will also be carried out. The numerical results shall be compared with the experimental data.
- Chapter 7 summarises the research results and proposes some possible research paths for the future study.

Fig. 1. 1 A typical offshore jacket structure (Wikipedia on oil platform, 2008)

Fig. 1. 2 Different types of tubular joints

Fig. 1. 3 Partially overlapped CHS K-joint configuration and notations

(a) Valid range of ζ

(b) Application ranges of overlap and gap K joints

Fig. 1. 4 Application ranges of overlap and gap K joints with $\theta_1 = \theta_2, \beta_1 = \beta_2$

Fig. 1. 5 Geometric parameters for non-eccentric joints ($\theta_1 = \theta_2, \beta_1 = \beta_2$)

Fig. 1. 6 Selection of overlap brace's geometric parameters for non-eccentric joints
(through brace known with $\theta_1 = 60^\circ, \beta_1 = 0.8$)

CHAPTER 2

LITERATURE REVIEW

2.1 INTRODUCTION

Fatigue is defined as the progressive damage and failure of materials under repeated cyclic loads (Dowling 1999). A meaningful estimation of the fatigue life of a tubular joint requires the appropriate assessment of its stress state around the joint intersection. It is because the HSS, SCF and SIF are major structural factors affecting the fatigue life of a tubular joint. Associated with the estimation of the SCF and SIF values, the HSS method and fracture mechanics method are two major approaches to evaluate the fatigue life of a tubular joint. The SCF is a major parameter in the traditional HSS method, which involves the calculation of the HSS and nominal stress. However, the HSS method does not distinguish the initiation and propagation phases of possible cracks but deals with the total life of the component. In contrast, the fracture mechanics method which requires the estimation of SIF provides a robust approach for the evaluation of the crack propagation phase and the determination of the remaining life of a tubular joint with existing crack.

For the estimation of SCF and SIF values in tubular joints, experimental and numerical analysis are two major approaches. In the experimental analysis, several techniques can be utilized, which vary from small scale to full scale tests; and from the measurement of HSS by strain gauges to the measurement of crack growth rate via ACPD technique. The experimental values can be used to develop semi-empirical models or verify the numerical model. It is because the numerical approach has proved to be powerful and cost effective. The numerical results can be used extensively and intensively for the study of fatigue behaviour of tubular joints. If a large number of specimens is involved in the numerical study, an automatic mesh generator based on the geometrical description of the tubular joint can be developed.

In this Chapter, the basic concepts of the HSS method and fracture mechanics method are reviewed. Existing research results on the evaluation of SCF in particular, and on the fatigue behaviour of partially overlapped CHS K-joints are reviewed. The fracture mechanics models for crack tubular joints are studied. Based on the reviewed literature, a research gap is identified for the assessment of fatigue behaviour of partially overlapped CHS K-joints.

2.2 HSS AND SCF IN CHS JOINTS

2.2.1 Hot spot stress in CHS joints

Due to the sudden change in geometrical profile, highly localized stress occurs at the joint intersection. Typical stress distribution in a simple T-joint is shown in Fig. 2.1. Besides the nominal stress on the brace, the localized stress along the joint intersection is defined as the HSS. The illustration of the stress rising at chord and brace sides of the weld is shown in Fig. 2.2. Although the calculation of HSS is specified in many design guidelines, unity for the definition of HSS has yet been reached. The Department of Energy (DEn 1993) based on the studies of Irvine (1981) accept the maximum principal stress around the non-welded brace/chord periphery. Whereas the International Institute of Welding (1996) and the American Petroleum Institute (2000), which are based on the studies of van Wingerde et al. (1996), adopt the HSS perpendicular to the weld path on the chord side. Although the perpendicular stress may not represent the exact stress state around the joint perimeter, the use of it would facilitate the experimental strain measurement because only simple strain gauges rather than strain rosettes is needed. Besides, the extrapolation of stresses and strains near the hot spots region can be easier and stresses from different load cases can be superimposed.

The value of HSS can be determined by experimental or numerical analysis. Although the strain gauges are an effective tool to measure the strain around the joint intersection, they cannot be positioned exactly at the weld path on the chord side. It is due to the limit in space and the actual unsmoothness of the weld path. On the other hand, for the numerical assessment of the HSS, when domain discontinuity occurs as in the case of the weld connecting two CHS, singularity problem exists in the stress

analysis. Therefore, in practice it has been accepted that the extrapolation method should be used to obtain the value of HSS for both experimental and numerical approach. In this method, the stress or strain is measured within the extrapolation boundary as illustrated in Fig. 2.3. The extrapolation boundary is determined such that out of it the effects of secondary local stress concentration like weld geometry and weld path irregularities are eliminated (CIDECT Guide 2000).

Generally, linear and quadratic extrapolations as described in Fig. 2.3, are specified in design guidelines. The extrapolation boundary according to the CIDECT Guide (Zhao et al. 2000) for each application is also plotted in the figure. The linear extrapolation is sufficiently accurate to be used with simple T/Y joints because the stress increase within the extrapolation region is fairly linear. However, for more complex joint such as the partially overlapped CHS K-joint, the quadratic extrapolation should be used to cater for the nonlinear stress distribution around the intersection periphery. The difference in SCFs obtained from the two methods can be up to 40% (van Wingerde et al. 1992).

2.2.2 Stress concentration factor in CHS joints

In many design guidelines (EuroCode3 2005, CIDECT Guide 2000), HSS are usually defined in terms of SCF with reference to the nominal stress σ_n where:

$$SCF = \frac{HSS}{\sigma_n}. \quad (2.1)$$

The nominal stress is calculated using simple beam theory and superposition method:

$$\sigma_n = \frac{F}{A} + \frac{M}{I} y, \quad (2.2)$$

in which the first term represents stress due to axial force and the second term corresponds to the bending moment.

The HSS for a general combined loading scenario can be calculated by the superposition method. According to the CIDECT Guide (Zhao et al. 2000), for all joints except CHS XX-joint under general loading condition, the total HSS can be determined by:

$$HSS = \sum_{i=1,3} \sigma_{LCi} SCF_{LCi} \quad (2.3)$$

where σ_{LCi} and SCF_{LCi} are the nominal stress and the corresponding SCF for basic load case i . The basic load cases are axial loading (AX), in-plane bending (IPB) and out-of-plane bending (OPB).

In the literature, a substantial amount of research effort has been spent to determine the HSS and SCF for several types of CHS joints. The covered area can be listed as the empirical parametric equations for T/Y-joints by Kuang et al. (1978), verified and extended for K/KT-joints by Wordsworth (1981). The SCF at the crown and saddle on the chord and brace sides of the weld by these studies were assessed by an extensive experimental study conducted by Smedley and Fisher (1991) under the support of the Lloyd's Register. The numerical parametric equation for T/Y-joints, gap and overlapped K-joints were developed by Efthymiou and Durkin (1985). The equations were assessed by Morgan and Lee (1997, 1998a and 1998b) for the gap K-joints. Numerical parametric equations for the SCF in T/Y-joints are also developed by Hellier et al. (1990a and 1990b) and Chang and Dover (1999a and 1999b). For more complex joints, research has been published in the work of Chiew et al. (1996, 1999) for the experimental investigation on a full scale XT-joint, Chiew et al. (2000a, 2000b) for the experimental investigation on a full scale XX-joint and Pang (2007) on four KK-joints. For partially overlapped CHS K-joints, except the numerical parametric equation developed by Efthymiou and Durkin (1985), the evaluation SCF has not been extensively and intensively investigated.

2.2.3 Parametric equation for the determination of SCF in partially overlapped CHS K-joints

To date, the analysis of SCFs for overlap K joint has yet been specified in any code of practice. The most recognised research work on SCFs of overlap K joints is presented by Efthymiou and Durkin (1985), in which the stress of over 100 partially overlapped CHS K-joints using the software PMBSHELL was analysed. Several load cases were considered and parametric equations were developed for each load case. However, large overlap could not be considered in this analysis due to the limitations of PMBSHELL mesh generator. In addition, the parametric equation only provides the

maximum SCF value when the specimen is loaded but not the SCF distribution along the joint intersection. This set of parametric equations was verified experimentally by Dharmavasan and Seneviratne (1986) through four strain-gauged acrylic models. The parametric equation formulae are summarised for the balanced load cases as follows:

- For the balanced AX loading,

- Chord:

$$SCF_{C, AX} = \tau^{0.9} \gamma^{0.5} (0.67 - \beta^2 + 1.16\beta) \sin \theta_{\max} \left[\frac{\sin \theta_{\max}}{\sin \theta_{\min}} \right]^{0.3} [1.64 + 0.29\beta^{-0.38} \arctan(8\zeta)] \quad (2.4)$$

- Brace:

$$SCF_{B, AX} = 1 + SCF_{C, AX} \times (1.97 - 1.57\beta^{0.25}) \tau^{-0.14} \sin^{0.7} \theta_{\max} + \beta^{1.5} \gamma^{0.5} \tau^{-1.22} \sin^{1.8} (\theta_{\max} + \theta_{\min}) [0.131 - 0.084 \operatorname{atan}(14\zeta + 4.2\beta)] \quad (2.5)$$

- For the balanced IPB loading,

- Chord:

$$SCF_{C, IPB} = 1.262\beta\tau^{0.55}\gamma^{(1-0.68\beta)} \sin^{0.7} \theta \quad (2.6)$$

- Brace

$$SCF_{B, IPB} = 1 + 0.48\beta^{0.24}\tau^{-0.2}\gamma^{0.7} \sin^{0.55} \theta_{\max} \left[\frac{\sin \theta_{\max}}{\sin \theta_{\min}} \right]^{1.8} \quad (2.7)$$

- For the balanced OPB loading,

- Chord:

$$SCF_{C, OPB} = K - K \times \left[0.88\beta^{0.5} \exp \left(-1.3 \left(1 + \frac{\zeta \sin \theta_A}{\beta} \right) \right) \right] \quad (2.8)$$

where:

$$K = \gamma\tau\beta(1.7 - 1.05\beta^3) \sin^{1.6} \theta \left[1 - 0.08\beta^{0.5}\gamma^{0.5} \exp \left(-0.8 \left(1 + \frac{\zeta \sin \theta_A}{\beta} \right) \right) \right] \quad (2.9)$$

- Brace:

$$SCF_{B, OPB} = SCF_{C, OPB} \times \left[\tau^{-0.54} \gamma^{-0.05} (0.99 - 0.47\beta + 0.08\beta^4) \right] \quad (2.10)$$

However, it is noted that the set of equation results in the maximum SCF value but does not specify the exact location of the SCF, as well as the SCF distribution along

the joint intersection.

2.2.4 Development of S-N curves for design

Although the S-N curve provides a practical approach for fatigue design, the construction of the S-N curve requires a large amount of experimental fatigue data from different geometry and materials. The first published S-N curve was presented in the AWS in 1972. The famous curve, also known as the X-curve, was constructed from the results of small scaled CHS joints and fillet welded plates tested in air. The current editions of AWS (1996) and API (2000) is still using the X-curve. Another S-N curve, namely the Q-curve, based on the same database, was published by the Department of Energy (UK). However, after being verified against subsequent experimental results, such as from the UK Offshore and Steel Program and the European Coal and Steel Community Program, the Q-curve was proved to lack conservatism under certain conditions. The Q-curve was then revised and published as the T-curve in the 1984 edition of DEn and it was recognised as the first S-N curve based on full scaled experimental results. Since then, additional tests have been conducted to assess the appropriateness of the T-curve. Until now, the T-curve has been revised once and adopted in several design guidelines (DEn 1990, IIW 1999, CIDECT Guide 2000 and EuroCode3 2005).

While the DEn T-curve was recommended as basic S-N curve of joints with thickness equal to 16mm, the thickness correction should be introduced for joints with wall thickness other than 16mm. While studying the results of fatigue tests on tubular joints, Holdbrook (1980) acknowledged that joints with similar HSS but different geometry or mode of loading often exhibit significantly different fatigue life. This difference can be attributed to crack growing patterns that in turn depend on the through thickness stress distribution and HSS. Therefore, for a rigorous model for fatigue failure and crack propagation in tubular joints, it is necessary to consider the distribution of stress through the tube wall thickness. Following this research direction, many investigation results have been developed, such as the works by Connolly et al. (1990), Pang and Lee (1995), and van Wingerde et al. (1997). The thickness correction formula was proposed based on the work of van Wingerde et al. (1997) and included in the IIW (1999) and the CIDECT Guide (Zhao et al. 2000). The full S-N curve with thickness correction is shown in Fig. 2.4 and the correction formula is given in Table 2.1.

In the application of the S-N curve for practical design, it is important that a failure criterion is defined. In general, it is considered detrimental when the depth of a crack a is equal to the thickness of the tube and the corresponding a number of loading cycles is N_f . However, the fatigue life of the specimen is normally defined as N_3 which is the number of cycles when through thickness crack is formed (DEn 1993, Bowness and Lee 1995, Chiew et al. 2004). It is because the determination of N_3 can be measured with smaller error compared with other failure criteria.

2.3 SIF AND THE FRACTURE MECHANICS APPROACH

2.3.1 Definition of the stress intensity factor

Linear elastic fracture mechanics (LEFM) is often used for the investigation of crack propagation. It is because the fatigue and fracture of tubular joints generally occurs at stresses appreciably below the yield stress of the material and under plane strain conditions (small scale yielding). LEFM is based on the concept that the stress near a crack tip is infinity large but has a proportional constant, named as the SIF and denoted as K . The stress field σ_{ij} at the crack tip can be written as (Westergaard 1939):

$$\sigma_{ij} = \frac{K}{\sqrt{2\pi r}} f_{ij}(\theta) + \text{non singular terms} \quad (2.11)$$

in which (r, θ) is the cylindrical coordinate of a point along the crack front and f_{ij} is a smooth and bounded function of (r, θ) . The stress σ_{ij} calculated by Eqn. (2.11) is said to have singularity of order $-1/2$. Alternatively K can be conveniently expressed as (Dowling 1999):

$$K = F \sigma_{ij} \sqrt{\pi c} \quad (2.12)$$

where F is a parameter depending on the specimen and crack geometry, and c is half of the crack length.

Due to different loading condition, the stress state around the crack tip is able to cause different crack surface displacement modes. LEFM categorized them as three crack extension modes as illustrated in Fig. 2.6. Mode I, referred to as the opening mode, is where the crack surfaces move directly apart. Mode II, known as sliding or in-plane

shear mode, is where the crack surface slides over one another in a direction perpendicular to the leading edge of the crack. Mode III, represents tearing or anti-plane shear mode, is where the crack surface move relatively to one another and parallel to the leading edge of the crack (Dowling 1999). Among the three mode, mode I loading is of major concern in actual engineering situation involving cracked components (Hertzberg 1996). Although a fatigue crack condition at the crack tip of a tubular joint often involves mixed mode loading, the effect of Mode II and Mode III is normally much smaller than that of Mode I. Using modifications of analytical methods described by Westergaard (1939), Irwin (1958) published solutions for crack tip stress distributions for the three modes. The stresses field ahead of a crack tip in Mode I in a coordinate system shown in Fig. 2.6 was written as follows:

$$\begin{aligned}\sigma_x &= \frac{K_I}{\sqrt{2\pi r}} \cos \frac{\theta}{2} \left(1 - \sin \frac{\theta}{3} \sin \frac{3\theta}{2} \right) \\ \sigma_y &= \frac{K_I}{\sqrt{2\pi r}} \cos \frac{\theta}{2} \left(1 + \sin \frac{\theta}{3} \sin \frac{3\theta}{2} \right) \\ \tau_{xy} &= \frac{K_I}{\sqrt{2\pi r}} \sin \frac{\theta}{2} \cos \frac{\theta}{3} \cos \frac{3\theta}{2}\end{aligned}\quad (2.12)$$

and the displacement field:

$$\begin{aligned}u &= 2(1+\nu) \frac{K_I}{E} \sqrt{\frac{r}{2\pi}} \cos \frac{\theta}{2} \left[1 - 2\nu + \sin^2 \frac{\theta}{2} \right] \\ v &= 2(1+\nu) \frac{K_I}{E} \sqrt{\frac{r}{2\pi}} \sin \frac{\theta}{2} \left[2 - 2\nu - \cos^2 \frac{\theta}{2} \right] \\ w &= 0\end{aligned}\quad (2.13)$$

where E denotes the Young's modulus of the material.

2.3.2 Fatigue crack growth rate and the Paris' law

Consider a growing crack that increase its depth by an amount Δa for a number of cycles ΔN . The fatigue crack growth rate is defined as the ratio $\Delta a/\Delta N$, or for small interval da/dN . Assume that the cyclic applied loading induce nominal stresses $\sigma_{\min}$ and $\sigma_{\max}$. The stress range $\Delta\sigma = \sigma_{\max} - \sigma_{\min}$ is used to calculate the range of SIF, which is considered the crack driving force by an equation similar to Eqn. (2.12). For a given material and test condition, experimental data have proved that there is a relationship

between the crack growth rate and the SIF range (Paris et al. 1972) as shown in Fig. 2.7. At the intermediate values of ΔK , the crack growth rate is relatively insensitive to the microstructure, mean stress and environmental effects. Paris and Erdogan (1963) concluded that an exponential relationship between the two parameters can be constructed, which is later known as the famous Paris' law:

$$\frac{da}{dN} = C(\Delta K)^m \quad (2.14)$$

where C is a constant and m is the slope on the log-log plot.

For low and high crack growth rate, the fitted curve in Fig. 2.7 is called the fatigue crack growth threshold. In the former region, crack will not ordinarily occur. In the latter, rapid unstable growth sometimes involves fully plastic yielding, in which the use of ΔK is improper. However, as most of the fatigue spends propagating cracks in the intermediate region and little fatigue life is left when a crack enters the latter region, Paris' law yields conservative results and is widely used.

2.3.3 Assessment of fracture mechanics parameters

Other than the SIF, the application of fracture mechanics method to study the crack growth involves other parameters such as the energy release rate J defined by Rice (1968) and the crack tip opening displacement proposed by Wells (1961) and Cottrell (1961). The energy release rate J (Rice 1968) was defined by a path-independence line integral around the crack tip as shown in Fig. 2.8 and written as:

$$J = \int_{\Gamma} \left(W dy - T \frac{\partial u}{\partial x} ds \right) \quad (2.15)$$

with:

$$W = W(x, y) = W(\varepsilon) = \int_0^{\varepsilon} \sigma_{ij} d\varepsilon_{ij} \quad (2.16)$$

and:

$$T_i = \sigma_{ij} n_j \quad (2.17)$$

where Γ is a clockwise closed contour around the crack front, T_i the traction perpendicular to Γ in the outside direction n_j and u is the displacement vector.

In small scale yielding, the relationship between the J -integral and SIF for mode I can be obtained as follows:

$$J = \frac{1-\nu^2}{E} K_I^2 \quad \text{for plane strain condition} \quad (2.18)$$

$$J = \frac{2}{E} K_I^2 \quad \text{for plane stress condition} \quad (2.19)$$

The J -integral is insensitive to mesh refinement and applicable to both elastic and plastic materials. However, it is noted that the J -integral is suitable characterizing only when an elastic singularity zone exists ahead of a crack tip, whereas in ductile material, there is quite large plastic zone ahead of a crack tip. Towards this end, Wells (1961) and Cottrell (1961) proposed that the crack tip opening displacement (CTOD) could be used to evaluate the crack extension. The CTOD can be used in elastic-plastic fracture mechanics for a wider range of crack sizes and stress applied. In numerical analysis the CTOD can be calculated from the relationship between the interpolation functions and the singularity relationship for displacement (Ingraffea and Manu 1980) as shown in Fig. 2.9 and written as:

$$v = \left[2v_B - v_C + 2v_E - v_F + v_D + \frac{1}{2}\eta(-4v_B + v_C + 4v_E - v_F) + \frac{1}{2}\eta^2(v_F + v_C - 2v_D) \right] \sqrt{\frac{r}{L_1}} \\ + \left[(\eta-1)(2v_B - v_C) - (1+\eta)(2v_E - v_F) \right] \frac{r}{L_1} \quad (2.20)$$

The numerical CTOD was proved to have a relationship (Ingraffea and Manu 1980) with the SIF as:

$$v = \frac{2(1-\nu^2)}{E} \sqrt{\frac{2r}{\pi}} K_I \quad (2.21)$$

2.4 METHODS FOR THE PREDICTION OF SIF

2.4.1 Experimental approach

The fatigue crack growth rate, da/dN , can be related to the SIF range by the Paris' law, which has been widely accepted as a valid representation of the fatigue crack growth

rate. In order to calculate the crack driving force K for a particular joint, besides the determination of the crack growth rate, the material parameters C and m should be known in advance. It is because any effects on the test results from the environment, frequency are assumed to be included in these material parameters. Towards this end, research effort has been largely spent in the 70s and 80s of the last century to study the variation of K with material, temperature and environment trends. It had been observed that in air and at room temperature the crack growth behaviour varies modestly within a narrowly defined class of material (Dowling 1999). In addition, rigorous database was built, regression analysis and statistical comparison were conducted (Yazdani and Albrecht 1989) and the values of C and m were unified for each class of material.

The experimental investigation on fatigue behaviour of CHS joints has been carried out for a number of decades. As far as in 1987, Tweed and Freeman presented the growth of surface crack of 100 small scale tubular joints under fatigue loadings. The effect of joint size, post weld heat treatment, immersion in sea water and variable amplitude loading had been assessed. It was shown that the stiffened and overlapped joints could have the ability of delaying the early crack growth when compared with relevant geometries of simple T-joints.

As far as the full scale test concerns, the research effort has been concentrated on the fatigue behaviour of simple CHS joints. In 1992, Maosheng et al. tested two full scale T-joints under fatigue loading. The crack growth rates were also assessed by an analytical method, in which the effect of weld geometry, plastic zone ahead of the crack tip and curvy shape of the crack front had been included. Equipped by more developed technique to capture the crack shape, Monahan and Dover (1995) presented the experimental procedure for two multi-planar M-joint and one X-joint. The detailed information obtained from the test was used to derive an empirical crack shape equation, which was useful for fatigue life calculation. In NTU, Huang (2003) and Ji (2006) had tested CHS and RHS Y-joints respectively, and Shao (2005) had presented test result for CHS gap K-joints. The number of tested specimens cannot be compared to what produced by numerical analysis; however, experimental tests are of vital role for the subsequent development of a tubular joint model. It can be seen from the aforementioned works that the experimental data have been effectively used to support

rigorous numerical analysis.

2.4.2 Semi-empirical models

From the experimental SIF from full scale tests, semi-empirical models have been produced in conjunction with the basic definition of K as shown in Eqn. (2.12). One of these widely used models was developed by Dover and Dharmavasan (1982). This model is also known as Average Stress Model (*AVS*). The function F in Eqn. (2.12) was determined as

$$F = A \left(\frac{t_0}{a} \right)^j, \quad (2.22)$$

where t_0 is the thickness of the CHS, A and j are functions of the SCF. A and j can be determined as follows:

$$A = 0.73 - 0.18 ASP, \quad (2.23)$$

$$j = 0.24 - 0.06 ASP. \quad (2.24)$$

Parameter ASP is defined as the ratio between the HSS and the average stress around the intersection of the CHS joint (Dover and Dharmavasan 1982). If the SCF is represented instead of these stresses, parameter ASP can be calculated as:

$$ASP = \frac{SCF_h}{SCF_{av}}, \quad (2.25)$$

where SCF_h and SCF_{av} represent the SCF according to the HSS and the average stress around the intersection of joint respectively. The value of SCF_{av} is given by the below Eqn. (2.26) for the case of IPB and Eqn. (2.27) for OPB and AX loading:

$$SCF_{av} = \frac{1}{\pi} \int_{-\frac{\pi}{2}}^{\frac{\pi}{2}} SCF(\alpha) d\alpha, \quad (2.26)$$

$$SCF_{av} = \frac{1}{\pi} \int_0^{\pi} SCF(\alpha) d\alpha, \quad (2.27)$$

where α is the driving angle around intersection ($\pi/2$ is the saddle position). The application range of the *AVS* model (Dover and Dharmavasan 1982) is stated as follows:

$$\begin{aligned}
0.21 &\leq \beta \leq 0.76, \\
2.66 &\leq SCF_h \leq 9.4, \\
1.51 &\leq SCF_{av} \leq 6.35, \\
16 &\leq t \leq 45 \text{ (mm)}.
\end{aligned}$$

Following this research path, Austin (1994) and Myers (1998) verified the model by full-scale tests. It was noted by Austin (1994) that this *AVS* model tended to over predict the experimental F factors for fatigue tests on axially loaded T-joints. Subsequently a reduction of 15% on the F factor was proposed to fit the experimental curve. Myers (1998) evaluated the two *AVS* models and found that especially the model improved by Austin (1994) could provide a good fit to experimentally derived K factors. Myers also pointed out that the difference between the *AVS* model and the experimental results in the previous research were caused by geometry effect rather than cycle counting method as suggested by Austin (1994).

Etube et al. (2000) also developed a new prediction model for the F factor in the *AVS* model. The equation was obtained from the same experimental database plus the results from the modified Newman and Raju (1981) flat plate solution. The F factor is calculated as:

$$F = A \left(\frac{t_0}{a} \right)^j - \exp \left[- \exp \left(A - \frac{a}{c} \right) \left(\frac{a}{t_0} \right) \right], \quad (2.28)$$

where:

$$A = 0.56 - 0.18 ASP, \quad (2.29)$$

$$j = 0.22 + 0.06 ASP, \quad (2.30)$$

and ASP calculated in Eqn. (2.25).

The semi-empirical models reviewed above were all produced using the data from the full-scale fatigue tests on CHS joints. The accuracy of the prediction is also based on the experimental database. For the joints having geometrical parameters fall within the validate range of the models, the semi-empirical models seems to be a good approach to predict the SIFs of the cracked CHS joint. However, for joints having geometrical

parameters fall outside the applicable range, the semi-empirical models should be used with caution. It is because full-scale fatigue tests are time consuming and expensive, therefore it becomes challenging to get the sufficient fatigue information for different types of CHS joints subjected to different basic loading cases.

2.4.3 Numerical modelling

Finite element (FE) method has been proved to be a reasonable method to predict the SIF values for CHS joints due to its great potential to accurately model the geometry, loading and structural restraint (Haswell and Hopkins 1991). In the early time, the numerical model had simulated the complete joint with shell finite elements and simplified the crack by line spring elements (Huang et al. 1988). However, simplified model was not able to cater for the important weld path on the chord side effects, Bowness and Lee (1995a) suggested that this model has limited application in CHS joints.

Subsequently the numerical model was improved to include solid elements at the positions of weld and crack while shell elements were applied to simulate the rest of the joint. In this approach, fatigue problem of CHS joints were published by Du & Hancock (1987) and Chong Rhee et al (1991). The fracture mechanics model provided by Chong Rhee et al (1991) was recommended for inclusion in the revised BS PD6493, in which the SIF solution was based on 40 FE models of cracked CHS T-joints generated by PRETUBE (1991). In the model, the intersection between the brace and chord was modelled using 20 node iso-parametric elements while the remainder of the joints was modelled using 8 nodes thick shell elements formed the remainder of the joint. The FE results were used to construct a parametric equation to predict the SIF in a form similar to (2.12) as:

$$K_e = \sigma_n Y_g Y_l Y_s \sqrt{\pi a}, \quad (2.31)$$

where Y_g is the joint geometry factor, Y_l is a crack size factor, Y_s is a joint and crack coupling factor, K_e is the equivalent SIF, and is defined as below:

$$K_e = \sqrt{K_I^2 + K_{II}^2 + \frac{K_{III}^2}{1-\nu^2}}. \quad (2.32)$$

The applicable range of parametric equation (2.31) is stated as below:

$$\alpha = 12$$

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

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

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

$$0.05 \leq \frac{a}{t_0} \leq 0.80 .$$

This solution only considers the situation that the crack is located at the saddle of the CHS T-joint and the valid range of the joint geometry and crack profiles is limited in the middle range of β . 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 path on the chord side.

As a result of availability of more powerful software and rapidly falling computational costs, full 3D solid element models were widely used to analyse CHS joints, such as in the studies published by Bowness and Lee (1995b), Qian(1996), Yang (1996) and Cao et al. (1998). The use of 3D solid element model has led to substantial improvement on the quality of the mesh for cracked CHS joints, especially for the mesh around the crack front. It is because the crack modelling involves a doubly curved crack front which curves not only around the joint intersection, but also under the weld path on the chord side (Bowness and Lee 1998). Normally, the solid mesh were discretized using the pre-processors incorporated in commercial softwares such as PRETUBE (1991), PATRAN (1993) and ANSYS (1996). This research path had been followed by Rhee et al. (1991) and Yang (1996) using the PATRAN and PRETUBE, and Qian et al. (1992) using the ANSYS. As a result, while the mesh without cracks could be obtained with ease, the modelling of crack was still a time-consuming and challenging task, and the mesh quality cannot be guaranteed (Cao et al. 1998).

In 1995, Bowness and Lee developed a set of programs named ABACRACK, ABABUTT and ABAMAP to generate meshes for surface cracks. The first program was used to generate a semi-elliptical crack on a plate surface, the second to produce a

3D butt mesh, the third to map the surface crack onto its position along the weld path. Cao et al. (1997, 1998) followed this philosophy and developed a mesh generation algorithm for surface cracked CHS T-joints. The method used a mapping procedure to project meshes on a plate onto the outer and inner surface of the CHS. The method was regarded as well defined in geometry and results obtained for uncracked CHS joints had high accuracy. However, for joints with cracks, the mesh around the crack front was badly distorted. The aspect ratios of some elements in this region even could exceed 1:50, which heavily affected the quality of SIF values for cracked CHS joints.

As far as the quality of regions around the crack front is concerned, it is found that the element types used to model this special position is essential. As in the work of Bowness and Lee (1995a) and Cao et al. (1998), singular prisms were used to model the crack front and hexahedral to fill up the volume elsewhere. Lie et al. (2003) found that only two types of elements, cracks could only be simulated for a limited configuration. Toward this end, five types of elements were employed in their mesh generation procedure. In fact, beside the singular prism used to model the crack front, the region connected to it was finely graded by hexahedral and normal pyramid elements. Other types of elements, such as tetrahedral and pyramid, were used to connect the meshes surrounding the crack front. Together with an algorithm on increasing the order and density of 3D finite element meshes by Lee et al. (2000), the quality of the mesh along the crack front, especially at the two ends, can be ensured.

Using the same method for mesh generation, it can be found in the literature the work of Ji (2005), Lie et al. (2004) and Shao (2004) for RHS T-joints, CHS Y-joints and CHS gap K-joints respectively. It has been concluded that the numerical results obtained using this mesh generation technique agree well with the experimental data (Huang 2003). More importantly, it only takes up to minutes of computational time for a tubular joint with cracks. This enhancement might facilitate the study of SIF for cracked CHS joints, which requires relatively high mesh quality at the crack front.

2.5 PREVIOUS WORKS ON PARTIALLY OVERLAPPED CHS K-JOINTS

It has been proved that partially overlapped CHS K-joints possess a high residual

capacity as a result of their optimized load transfer pattern (BOMEL 1992). In addition in Section 1.3, it is also proved that the use of partially overlapped K-joints is essential for certain combination of geometrical parameters. Nevertheless, few research works has been found for the behaviour of partially overlapped CHS K-joints. The covered area includes the investigation on the ultimate strength, the SCF and SIF.

Pioneering in the research path concerning the ultimate strength of partially overlapped CHS K-joints, Kurobane et al. (1984) screened a database of AX loaded K-joints. The database included tests where significant ductile chord plastic deflection occurred. The alternative failure mode of brace local buckling was investigated in Kurobane et al. (1986). It was recognized that for partially overlapped CHS K-joint, both brace local buckling and chord bending occur simultaneously in many cases. The CIDECT (2000) provisions were generally based on the conservative simplification of the Kurobane recommendations. Other than AX loading condition, the IPB loading condition was selected and studied by Healy (1994) in a limited parametric study of CHS K-joint. A matrix of six joint configurations was investigated. The research had pointed out some drawbacks of the current design guidelines when they treated the partially overlapped CHS K-joint as an assembly of simple T/Y-joints. It was also stressed that when the through brace was in tension, the capacity of the partially overlapped joint was much better than when it was otherwise loaded. The strength of the partially overlapped CHS K-joints was found to be approximately 1.25-2.25 larger than that of the corresponding gap joints.

Dexter and Lee (1999a and 199b) numerically studied the static strength of AX loaded partially overlapped CHS K-joints. The effect of several geometrical parameters and the overlap amount on the behaviour of the overlapped joints were examined. The results of some CHS K-joints with small gaps were also included for comparison purpose. Overlapping the braces was shown to have a beneficial effect on joint strength for the joints with relatively thick brace walls, where the strength was found to be up to 4.5 times that of the corresponding gap joint. However, it was also reported from Dexter et al. (1996) that partially overlapped CHS K-joints are expected to have increased strength up to a certain amount of overlap and then decrease when the joint tends to be completely overlapped.

In the research path concerning the SCF of partially overlapped CHS K-joints, the most recognised work was published by Efthymiou and Durkin (1985). The authors had analysed the HSS of over 100 partially overlapped CHS K-joints using the FE package PMBSHELL. Nine load cases were considered and parametric equations were developed for each load case. The positions where SCF likely occur were highlighted. It was concluded that under balanced AX and OPB loading, overlapping helps reduce the chord SCFs significantly, where for IPB, the SCF resemble those in corresponding T/Y joints and overlapping is not beneficial. The accuracy of the result was verified against Kuang et al. (1978) and Wordsworth and Smedley (1978) over a small number of T/Y joints. However, large overlap could not be considered in this analysis due to the limitations of the PMBSHELL mesh generator. This set of parametric equations was verified experimentally by Dharmavasan and Seneviratne (1986) through four strain-gauged acrylic models. Generally, the positions and magnitude of SCF obtained from the experiment agreed well with those calculated from Efthymiou and Durkin (1985). However, as high SCF was detected in the crown toe of the through brace, which is the position of hidden weld, higher safety factor was proposed. Deliberate inspection of the effect of the triple point on the stress distribution in different load cases was also presented.

For the investigation on the SIF of partially overlapped CHS K-joints, only qualitative conclusion was published. The crack growth rate, an important factor to assess the fatigue performance of a CHS joint, could not be recorded due to the lack of equipment. However, interesting notes on the fatigue behaviour of this particular joint was published, such as in the research by Wylde (1981). Some modes of fatigue crack development observed in a partially overlapped CHS K-joint tested under OPB loading were described. It was generally concluded that fatigue cracks started very early on tubular joints (at around 10% of the total life) and the overall stiffness only reduce significantly before through thickness crack initiated. Besides that, Lalani and Forsyth (1987) presented the results of six fatigue tests performed on partially overlapped N-joints. Rigorous data on the crack lengths and depths were recorded. The main conclusions drawn from the study are: (i) The peak HSS does not always occur at the crown or saddle position. (ii) All cracks during the fatigue tests occurred on the overlap brace side of the specimen, in line with the maximum stress locations. (iii) A number of crack initiation sites exist simultaneously.

2.6 CONCLUDING REMARKS

The HSS method and the fracture mechanics method are popular approach to assess the fatigue performance of CHS joints. It has been well known that partially overlapped K-joints represent a heavy duty joint type which can be utilised in offshore and bridge structures. In addition, it is also shown that the use of this kind of joint is essential and even unavoidable in certain combination of geometrical parameters. Although fatigue assessment is important for a structure subjected to repetitive nature of loadings, few research works has been found for the fatigue behaviour of partially overlapped CHS K-joints. For the investigation concerning the SCF values, the most recognised work was published by Efthymiou and Durkin (1985). However it is noted that the set of equation only provides estimation on the maximum SCF value but not the location of the HSS. For the investigation concerning the SIF values, the accurate estimation of the SIF for cracked partially overlapped CHS K-joints is still a relatively new research area. Furthermore, as far as the numerical analysis is concerned, the automatic mesh generation approach has been reported to be the appropriate trend to investigate the SCF and SIF of tubular joints in a large number of specimens. In practice, for partially overlapped K-joint, much difficulty has been encountered when one attempts to use commercial software such as ANSYS for the generation for a joint with large percentage of overlapping. Therefore, the development of a consistent and reliable mesh generation procedure which is able to cover for a wide range of parameter in order to facilitate the numerical analysis of partially overlapped CHS K-joint is chosen to be the research path of this study.

Table 2. 1 Equations for the S-N curves for CHS joints (4 mm ≤ t ≤ 50 mm) and RHS joints (4 mm ≤ t ≤ 16 mm) (IIW 1999 and CIDECT Guide 2000)

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

Fig. 2. 1 Stress distribution at the intersection of a T-joint

(a) Stress distribution in chord

(b) Stress distribution in brace

Fig. 2. 2 Definition of hot spot stress in tubular joint (IIW 1999)

Fig. 2. 3 Linear and quadratic extrapolation procedures (CIDECT Guide 2000)

Fig. 2. 4 S-N curve for CHS joints (CIDECT Guide 2000)

Fig. 2. 5 Crack growth for superimposed mixed mode loadings

Fig. 2. 6 Stresses along the crack front

Fig. 2. 7 Fatigue crack growth rates for a ductile steel (Paris 1972)

- (a) Slow growth near the threshold K
- (b) Intermediate region following a power equation
- (c) Unstable rapid growth

Fig. 2. 8 Crack tip opening displacement for the determination of SIFs

Fig. 2. 9 Crack tip opening displacement for the determination of SIFs

CHAPTER 3

GEOMETRICAL MODELLING OF PARTIALLY OVERLAPPED CHS K-JOINTS

3.1 INTRODUCTION

The development of a well defined, consistent and realistic geometrical model for tubular joints is important. It is because the mesh generation, one of the essential steps leading to a successful FE analysis, depends profoundly on an accurate underlying geometrical model. However, most of researches in the literature (Healy and Buitrago 1994, Fung et al. 2001) were carried out using meshes generated by general-purposes commercial software such as ABAQUS, ANSYS or MARC. In particular, for the study of ultimate strength of partially overlapped CHS K-joints by Healy (1994) and Dexter and Lee (1999a), while the numerical analyses were performed for a great number of specimens, very few discussions on the geometry of tubular joints were given. In fact, when welding and crack details are to be considered for a large number of partially overlapped K-joint specimens, the use of general-purposed mesh generators might become time and cost consuming.

Recently, Lee et al. (1999) and Lie et al. (2003) developed consistent geometrical models with welding details for tubular T/Y-joints and gap K-joints. The analytical intersection curve was based on a geometrical analysis by Cao et al. (1997); which utilize mapping formulae to transform a circular tube into a plane. The weld paths were developed based on a detailed study of the dihedral angle. The method is proved to be consistent, accurate and flexible. Therefore, it is employed for the modelling of partially overlapped CHS K-joints. In this Chapter, the implementation of this method for partially overlapped CHS K-joints is explained. Some additional formulae for the reverse mapping of a plane into a circular tube and an amendment to the weld model are presented. The weld model is also extended for the case when the chord and brace are of equal diameter. Furthermore, an algorithm is developed for the determination of

the theoretical intersection points between the braces and the chord, which is a distinctive feature of partially overlapped CHS K-joints. Subsequently, the actual intersection points, which present the common area where several weld paths meet, can be identified in a similar manner.

For tubular joints, the weld thickness inevitably affects the size, and hence the stiffness of the joint and may lead to a reduction in the strength of the joint and even possible changes of the overall behaviour (Lee 1999). In Section 3.5, a consistent weld model which conforms to the specification of weld thickness in codes of practice (the AWS 1996 and the API 2000). However, due to the complex configuration of a partially overlapped CHS K-joint, it is necessary to validate the existing geometrical model against the actual measurement on some small scale joints. Parameters in the weld model are then adjusted as described in Section 3.7.

3.2 DEFINITION OF AN INTERSECTION CURVE

For the determination of intersection curve between the chord and the brace, it is necessary to establish the analytical expression of the objects intersected. The chord and the brace are described in their local coordinates (Fig. 3.1) as:

$$X^2 + Y^2 = R^2 \quad (3.1)$$

$$x^2 + y^2 = r^2 \quad (3.2)$$

The relationship between the two coordinate systems is given by:

$$\begin{cases} X = x \cos \theta + z \sin \theta + R \\ Y = y \\ Z = z \cos \theta - x \sin \theta \end{cases} \quad (3.3)$$

$$\text{or} \quad \begin{cases} x = (X - R) \cos \theta - Z \sin \theta \\ y = Y \\ z = (x - R) \sin \theta + Z \cos \theta \end{cases} \quad (3.4)$$

The brace can be expressed in the XYZ coordinate system through the transformation described in Eqn. (3.4):

$$\left[(X - R) \cos \theta - Z \sin \theta \right]^2 + Y^2 = r^2 \quad (3.5)$$

The chord can either be represented in explicit form as in Eqn. (3.1) or in another

parametric form, which is obtained by introducing a mapping process to transform the plane $X = R$ onto the CHS described by Eqn. (3.1) as shown in Fig. 3.2. The mapping process is described as follows:

$$R\psi = Y' \quad (3.6)$$

where ψ is the local polar angle defined on the chord cross section.

The parametric form of the chord equation is written as:

$$\begin{cases} X = R \cos\left(\frac{Y'}{R}\right) \\ Y = R \sin\left(\frac{Y'}{R}\right) \end{cases} \quad (3.7)$$

When a CHS is unwrapped onto a plane, a reverse transformation of Eqn. (3.7) is needed. This reverse transformation is given by Cao et al. (1997) as:

$$Y' = R \arcsin\left(\frac{Y}{R}\right) \quad (3.8)$$

It is noted that Eqn. (3.8) is only valid for the upper part of the CHS ($X \geq 0$). The use of Eqn. (3.8) may result in wrong transformation if the lower part of the CHS is involved. Through geometrical observation, the complete reverse transformation may be written as follows:

$$\begin{cases} \text{For } X \geq 0 : Y' = R \arcsin\left(\frac{Y}{R}\right) \\ \text{For } X < 0 : Y' = \pi R - R \arcsin\left(\frac{Y}{R}\right) \text{ when } Y \geq 0 \\ \quad \quad \quad Y' = -\pi R - R \arcsin\left(\frac{Y}{R}\right) \text{ when } Y \leq 0 \end{cases} \quad (3.9)$$

The illustration for this reverse transformation is pictured in Fig. 3.3. It is shown that the mapping represented in Eqn. (3.6) is similar to a folding procedure, whereas Eqn. (3.9) described a unfold step, through which point B on the CHS is transformed into 2 points B₁ and B₂ in the $Y'Z'$ plane. Eqn. (3.9) can be applied to unfold the through and overlap braces, where intersection curves reside on both the lower and upper parts of the CHS. With the use of Eqn. (3.9), the generation of the surface meshes for the through and overlapped braces as described in Section 4.3 is possible.

The intersection curve between the chord and brace should satisfy both Eqns. (3.1) and (3.2), or, in other word, both Eqns. (3.5) and (3.7). Substituting Eqn. (3.7) into Eqn.

(3.5), the equation for the intersection curve yields:

$$R^2 \sin^2 \frac{Y'}{R} + \left[Z' \sin \theta + R \left(1 - \cos \frac{Y'}{R} \right) \cos \theta \right]^2 = r^2 \quad (3.10)$$

Observed that Eqn. (3.10) has the form of a circle, Cao et al. utilized this expression to conveniently define the intersection curve as a double mapping (Fig. 3.4), in which the second step is expressed by Eqn. (3.7), and the first step is described as follows:

$$u^2 + v^2 = r^2 \quad (3.11)$$

$$\begin{cases} u = r \sin \alpha \\ v = r \cos \alpha \end{cases} \quad (3.12)$$

By combining Eqns. (3.10) and (3.12), the intersection curve can be obtained from the double mapping step as:

$$\begin{cases} Y' = R \arcsin \left(\frac{u}{R} \right) \\ Z' = \left[v - R \left(1 - \cos \frac{Y'}{R} \right) \cos \theta \right] \frac{1}{\sin \theta} \end{cases} \quad (3.13)$$

The relationship in Eqn. (3.7) is used to map the intersection curve described in Eqn. (3.13) from the plane $Y'Z'$ to the surface of the chord. After mapping, the intersection curve can be written in the XYZ coordinate system in parametric form as:

$$\begin{cases} X = R \cos \left(\arcsin \frac{u}{R} \right) \\ Y = R \sin \left(\arcsin \frac{u}{R} \right) = u \\ Z = \left[v - R \left(1 - \cos \frac{Y'}{R} \right) \cos \theta \right] \frac{1}{\sin \theta} \end{cases} \quad (3.14)$$

In Fig. 3.4, the angle ϕ corresponding to the driving angle α can be obtained by:

$$\phi = \arctan \left(\frac{Y}{Z} \right) \quad (3.15)$$

3.3 PARAMETERS FOR WELD PROFILE MODELLING

3.3.1 Dihedral angle γ

The AWS (2008) defines the dihedral angle as the angle measured in a plane perpendicular to the line of the weld, between the tangents to the outside surface of the CHSs being joined at the weld. In the AWS (1996) and the API (2000), the method of welding and thickness of the weld are determined by the dihedral angle along the joint perimeter. The formulae to calculate the dihedral angle for any point located at the joint intersection are adopted from Suen and Wu (1993).

The definition of the dihedral angle γ is illustrated in Fig. 3.5. Supposed that $\mathbf{n}_1$ and $\mathbf{n}_2$ are two normal vectors of the tangential planes corresponding to the chord and brace, respectively, at a certain point on the intersection curve, the dihedral angle γ is calculated via $\mathbf{n}_1$ and $\mathbf{n}_2$ as:

$$\gamma = \pi - \cos^{-1} \left[\frac{\mathbf{n}_1 \mathbf{n}_2}{\|\mathbf{n}_1\| \|\mathbf{n}_2\|} \right] \quad (3.16)$$

As the outer surface of the chord can be expressed in Eqn. (3.1), when this expression is differentiated with respect to Y , the surface gradient is defined as:

$$\frac{dX}{dY} = -\frac{Y}{X} \quad (3.17)$$

Because the tangential surface S_c at point A of the chord is parallel to the axis Z , its equation in the XYZ coordinate system can be expressed in the form of a tangent line in the XY plane as:

$$X = m_t Y + k \quad (3.18)$$

where k is a constant and m_t is the surface gradient of the tangential plane described by Eqn. (3.17).

The surface gradient at point A can be obtained by substituting the relevant coordinate (X_A , Y_A , Z_A) into Eqn. (3.17):

$$m_t = -\frac{Y_A}{X_A} \quad (3.19)$$

By a similar approach, k can be written as:

$$k = \frac{X_A^2 + Y_A^2}{X_A} = \frac{R^2}{X_A} \quad (3.20)$$

By substituting Eqns. (3.19) and (3.20) into Eqn. (3.18) and rearranging, the definition of the tangential surface S_c at the point A in the XYZ coordinate system is given by:

$$\left(\frac{X_A}{R^2}\right)X + \left(\frac{Y_A}{R^2}\right)Y = 1 \quad (3.21)$$

The tangential surface equation for the chord can be written in a general form as:

$$A_1X + B_1Y + C_1Z = D_1 \quad (3.22)$$

Comparing Eqn. (3.22) to Eqn. (3.21) one get:

$$A_1 = \left(\frac{X_A}{R^2}\right) \quad B_1 = \left(\frac{Y_A}{R^2}\right) \quad C_1 = 0 \quad D_1 = 1 \quad (3.23)$$

Similarly, the tangential surface, S_b , of the chord at point A can be defined in the xyz coordinate system as:

$$\left(\frac{x_A}{r^2}\right)x + \left(\frac{y_A}{r^2}\right)y = 1 \quad (3.24)$$

By taking the transformation described in Eqn. (3.4) to represent Eqn. (3.24) in the XYZ coordinate system, one has:

$$X\left(\frac{x_A}{r^2}\right)\cos\theta + Y\left(\frac{y_A}{r^2}\right) - Z\left(\frac{y_A}{r^2}\right)\sin\theta = \left(\frac{x_A}{r^2}\right)R\cos\theta + 1 \quad (3.25)$$

The general form of Eqn. (3.25) is:

$$A_2X + B_2Y + C_2Z = D_2 \quad (3.26)$$

In which the parameters are defined by comparing Eqn. (3.26) with Eqn. (3.25):

$$A_2 = X\left(\frac{x_A}{r^2}\right)\cos\theta \quad B_2 = \left(\frac{y_A}{r^2}\right) \quad C_2 = Z\left(\frac{y_A}{r^2}\right)\sin\theta \quad D_2 = \left(\frac{x_A}{r^2}\right)R\cos\theta + 1 \quad (3.27)$$

The normal vectors $\mathbf{n}_1$ and $\mathbf{n}_2$ are deduced from Eqns. (3.23) and (3.27) as:

$$\mathbf{n}_1 = A_1\mathbf{i} + B_1\mathbf{j} \quad (C_1 = 0) \quad (3.28)$$

$$\mathbf{n}_2 = A_2\mathbf{i} + B_2\mathbf{j} + C_2\mathbf{k} \quad (3.29)$$

By substituting the value of vector multiplication and vector lengths of $\mathbf{n}_1$ and $\mathbf{n}_2$ into Eqn. (3.16), the dihedral angle γ can be determined.

3.3.2 Projection angle β_a

Angle β_a is defined as the angle between the normal of the intersecting curve at point A and the horizontal Z -axis in the YZ plane as shown in Fig. 3.6. The angle β_a is used as a parameter to project the analytical intersection curve to the weld paths in the direction normal to them. Consider a tangent line formed as the result of the intersection between the planes represented by Eqn. (3.25) and $X = X_A$ as shown in Fig. 3.6. In the $Y'Z'$ plane, the slope of this line can be obtained by partial differentiating Eqn. (3.24) with respect to Y :

$$\left(\frac{y_A}{r^2} \right) - \frac{\partial Z}{\partial Y} \left(\frac{x_A}{r^2} \right) \sin \theta = 0$$

$$\frac{\partial Z}{\partial Y} = \left(\frac{y_A}{x_A \sin \theta} \right) \quad (3.30)$$

The gradient of the normal to the intersecting curve at point A , m_{normal} , in the $Y'Z'$ plane can be calculated as:

$$m_{\text{normal}} = \frac{1}{\partial Z / \partial Y} = - \frac{x_A \sin \theta}{y_A} \quad (3.31)$$

As a result, β_a can be given by:

$$\beta_a = \frac{\pi}{2} - \arctan \left(- \frac{x_A \sin \theta}{y_A} \right) \quad (3.32)$$

However, it should be noted that β_a was calculated in the $Y'Z'$ plane based on the tangential line to the mapped intersection curve, not the actual 3D curve.

3.4 THE AWS AND API CRITERIA FOR COMPLETE JOINT PENETRATION

For the modelling of weld thickness, either the specifications by the API (2000) or the AWS (1996) can be adopted. In this research, the requirement in the AWS (1996) is studied and applied. Theoretically, the minimum weld thickness specified in the AWS (1996), T_{AWS} , is calculated from the thickness of the brace multiplied by a factor k_{AWS} as shown below:

$$T_{AWS} = k_{AWS} \times t_b \quad (3.33)$$

The value of the factor k_{AWS} along the joint intersection depends on the dihedral angle γ as shown in Table 3.1 and detailed in Appendix A.

Obviously, the actual welding thickness of a complete joint on the chord side, T_w , is required to be larger than T_{AWS} , that is:

$$T_w \geq T_{AWS} \quad (3.34)$$

3.5 GEOMETRICAL MODEL OF THE WELD

The shape of the weld profile around a particular intersection curve is shown in Fig. 3.7. In order to model the weld with smooth path, Wong (2000) made an elaborate investigation on the behaviour of the dihedral angle. However, as the weld model proposed by Wong (2000) was limited for tubular joints with the ratio $r/R = 0.8$, the case $r/R = 1.0$ is not uncommon for partially overlapped K-joints, since the dimensions of the through and overlap braces are often the same. In addition, as observed in fatigue full scale tests of partially overlapped CHS K-joints (Lee et al. 2007a), the weld thickness on the brace side should also be carefully investigated, as the HSS and crack occurred at that position. Therefore, the weld model proposed by Wong (2000) is adjusted and extended to cater for such cases and is presented in this Section.

3.5.1 Assumptions

In order to obtain a feasible geometrical modelling of the weld, as well as maintaining a smooth weld path, the below assumptions have been made:

- The thickness of brace t_b is small compare to the outer radius of the chord R .
- The smallest intersecting angle between the brace and chord members is 30° .
- The weld would be extended with extra thickness at the joint when γ greater than 135° though it is not necessary (T_w need not exceed $1.75t_b$ by the AWS specifications).
- The material properties of weld are same as the tubular members. Hence, the gap or root as defined in the AWS (2008) is not important in the modelling.

3.5.2 Original contact thickness T_1

For intersection of two CHS without welding details, the original contact thickness T_1 is the distance between the outer and the inner intersection curves in the direction normal to them. Theoretically, T_1 varies along the intersection as a function of the dihedral angle. To simplify the problem, it is assumed that a flat surface connects the inner and outer intersection curves. Then it follows that $\gamma_o = \gamma_i = \gamma$. T_1 therefore can be approximated as:

$$T_1 = k_1 \times t_b \quad (3.35)$$

$$\text{where } k_1 = \frac{1}{\sin \gamma}.$$

3.5.3 Geometrical modelling on the chord side of the weld

On the chord surface, there are two weld paths named as the outer and inner welds on the chord side. They are projected from the original intersection curves as shown in Fig. 3.7. Along the outer intersection curve, the projection distance T_2 varies along with the outer dihedral angle γ_o to form the weld path on the chord side curve. As shown in Fig. 3.8, T_2 can be calculated by the brace thickness and a modification factor (Wong 2000) as:

$$T_2 = k_2 \times t_b \quad (3.36)$$

$$k_2 = F_{OS_{outer}} \left[1 - \left(\frac{\gamma_o - \theta_s}{\pi - \theta_s} \right)^{n_2} \right] \quad (3.37)$$

where T_2 is the modified outer thickness, k_2 is the modification factor of the outer intersection curve, $F_{OS_{outer}}$ is a scale factor, n_2 is a constant, θ_s is the smallest intersecting angle (30°) as stated in the assumptions.

The parametric equation of the outer weld on the chord side, according to Wong (2000), can be written as:

$$\begin{cases} Z_{w_0} = Z_{A_0} + T_2 \cos \beta_0 \\ Y_{w_0} = Y_{A_0} + T_2 \sin \beta_0 \\ X_{w_0} = \sqrt{R_1^2 - Y_{w_0}^2} \end{cases} \quad (3.38)$$

However, as noted in Section 3.3.2, β_0 is calculated in the $Y'Z'$ plane, therefore, the weld thickness on the chord side should be calculated firstly in the $Y'Z'$ plane and then mapped to the surface of the chord using Eqn. (3.38). The parametric equation for the outer weld on the chord side, hence, is given by:

$$\begin{cases} Z'_{w_0} = Z'_{A_0} + T_2 \cos \beta_0 \\ Y'_{w_0} = Y'_{A_0} + T_2 \sin \beta_0 \\ X_{w_0} = R \cos \frac{Y'_{w_0}}{R} \\ Y_{w_0} = R \sin \frac{Y'_{w_0}}{R} \\ Z_{w_0} = Z'_{w_0} \end{cases} \quad (3.39)$$

The adjustment of the outer weld on the chord side is illustrated in Fig. 3.9. In this figure, the models described in Eqns. (3.38) and (3.39) are used to reproduce the outer intersection curve from the inner intersection curve by projecting a length equal to the original contact thickness at each position. The same parameter $F_{OSouter}$, m , and θ_s are used for both models. It is seen that when using Wong's model (Fig. 3.9a), the outer intersection curve cannot be reproduced exactly: there still is some excessive thickness beyond the exact intersection curve especially at the saddle position. The excessive thickness becomes more severe as the ratio r/R increases. In contrast, as shown in Fig. 3.9b, the adjusted model described in Eqn. (3.39) demonstrates that the outer intersection curve can be reproduced accurately. Therefore, Eqn. (3.39) is used to model the outer weld on the chord side in subsequent work in this research.

Similar to the outer weld model, the geometrical model for the inner weld on the chord side can be formed when the inner intersection curve is shifted a distance T_3 in the

direction perpendicular to it:

$$T_3 = k_3 \times t_b \quad (3.40)$$

$$k_3 = Fos_{inner} \left[1 - \left(\frac{\gamma_i - \theta_s}{\pi/2 - \theta_s} \right)^{n_3} \right] \quad (3.41)$$

where T_3 is the modified inner thickness, k_3 is the modification factor of the inner intersection curve, Fos_{inner} is a scale factor, n_3 is a constant, θ_s is the smallest intersecting angle (30°) as stated in the assumptions.

Hence, the geometrical model for the inner weld on the chord side can be written in a parametric form as:

$$\begin{cases} Z'_{wi} = Z'_{Ai} + T_2 \cos \beta_i \\ Y'_{wi} = Y'_{Ai} + T_2 \sin \beta_i \\ X_{wi} = R \cos \frac{Y'_{wi}}{R} \\ Y_{wi} = R \sin \frac{Y'_{wi}}{R} \\ Z_{wi} = Z'_{wi} \end{cases} \quad (3.42)$$

The total weld thickness on the chord side for a particular intersection curve can be determined by adding T_1 , T_2 and T_3 together:

$$T_W = T_1 + T_2 - T_3 \quad (3.43)$$

If k_{Tw} is defined as:

$$k_{Tw} = (k_1 + k_2 - k_3), \quad (3.44)$$

then the welding thickness can be expressed in a compact form as:

$$T_W = k_{Tw} t_b. \quad (3.45)$$

T_W should satisfy the AWS requirement, therefore it is essential that $T_W \geq T_{AWS}$.

In the geometrical model for the outer and inner weld on the chord side, parameters FOS_{outer} , FOS_{inner} , n_2 , n_3 can be determined by avoiding values leading to kinky shapes of the weld thickness and by forcing the weld model to be equal to the standardized values at some special positions (Wong 2000). The appropriate ranges for these parameters are listed as follows:

$$\begin{aligned}
Fos_{\text{outer}} &\geq 0.3 \text{ (typically, } Fos_{\text{outer}} = 0.3) \\
0 \leq Fos_{\text{inner}} &\leq 0.25 \text{ (typically, } Fos_{\text{inner}} = 0.25) \\
n_2 &= 2.0 \\
n_3 &= 0.4
\end{aligned} \tag{3.46}$$

However, it is noted that parameters for the weld model on the brace side, as well as weld thickness for the case $r/R = 1.0$, has yet to be examined.

3.5.4 Weld thickness on the chord side of the weld for the case $r/R = 1.0$

When the welding details are included, the presence of member thickness must be considered in the geometrical model. For this reason, two intersection curves, the inner and the outer intersection curves, and their associated inner (γ_i) and outer (γ_o) dihedral angles are needed to be considered. As shown in Fig. 3.10a and 3.11, γ_i and γ_o exhibit similar behaviours. The difference between them only increases when the ratio r/R is large and becomes severe when $r/R = 1.0$, at which the outer dihedral angle γ_o achieves its maximum of π as shown in Figs. 3.10b and 3.11. Because the expression for k_1 involves $\sin \gamma$ in the denominator, the use γ_o of for γ leads to infinite value of k_1 , which in fact is not real. Therefore, γ_i is used subsequently for the expression of the original contact thickness.

The weld shape and weld thickness for the case $r/R = 1.0$ is plotted out for the case of a T-joint in Figs. 3.12 and 3.13 respectively. It can be seen that model satisfies the AWS requirements. However, it is too conservative compared to the AWS (2008) requirement at $\alpha = \pi/2$ where $T_W = 3.29t_b \gg T_{AWS} = 1.75t_b$, which is stated that when the dihedral angle γ_o exceeds $3\pi/4$, the weld should be built to full contact thickness but need not exceed a certain limit of $1.75t_b$. To meet this criterion, another reduction when γ_o varies in the range of $[3\pi/4, \pi]$ should be introduced to the modelling of the outer weld path. After considering several alternatives for the factor k_2 , it is modified as:

$$k_2 = \begin{cases} Fos_{\text{outer}} \left[1 - \left(\frac{\gamma_i - \theta_s}{\pi - \theta_s} \right)^{n_2} \right], & \gamma_o \leq 3\pi/4 \\ a\gamma_i + b - \frac{1}{\sin \gamma_i} & 3\pi/4 \leq \gamma_o \leq \pi \end{cases} \tag{3.47}$$

in which:

$$\begin{aligned}
 a &= \frac{\cos \gamma_i}{\sin^2 \gamma_i} \Big|_{\gamma_o=3\pi/4} - \frac{n_2 F_{\text{OSouter}}}{0.75\pi - \theta_s} \left(\frac{0.75\pi - \theta_s}{\pi - \theta_s} \right)^{n_2-1} \\
 b &= F_{\text{OSouter}} \left[1 - \left(\frac{0.75\pi - \theta_s}{\pi - \theta_s} \right)^{n_2} \right] + \frac{1}{\sin \gamma_i} \Big|_{\gamma_o=3\pi/4} - a \gamma_i \Big|_{\gamma_o=3\pi/4}
 \end{aligned} \tag{3.48}$$

To facilitate the process of mesh generation around the joint intersection for partially overlapped CHS K-joints, the *modelled* intersection curve is defined as a 3D curve positioned on the outer surface of the chord's wall having a distance k_2 to the outer weld path as shown in Fig. 3.12. Generally, the *modelled* intersection curve coincides with its *actual* counterpart, except for cases with large r/R ratio leading to large dihedral angles. The formula for the *modelled* intersection curve, or k_1 value, can be written as follows:

$$k_1 = \begin{cases} \frac{1}{\sin \gamma_i}, & \gamma_o \leq 3\pi/4 \\ a\gamma_i + b & 3\pi/4 \leq \gamma_o \leq \pi \end{cases} \tag{3.49}$$

In Eqn. (3.49), parameters a and b can be obtained from Eqn. (3.48). The final geometrical modelling of weld thickness for a T-joint with $r/R = 1.0$ is shown in Fig. 3.13. In this proposed model for the weld thickness on the chord side, welding parameters F_{OSouter} , F_{OSinner} , n_2 , n_3 are kept similar to Wong's model. Fig. 3.13 also compares the welding thickness on the chord side for Wong's model, the proposed model, as well as the AWS and API requirements for a tubular T-joint. With a maximum difference between the two models of 27%, it can be concluded that the proposed weld model is able to significantly reduce the over-profile problem for the geometrical modelling of the weld thickness on the chord side. The shape of the outer weld using the proposed model is shown in Fig. 3.12. Verification of the proposed weld model is given in Section 3.7.

3.5.5 Weld thickness on the brace side of the weld

For partially overlapped CHS K-joint, the HSS and therefore fatigue crack could appear anywhere along the joint intersection. More interestingly, they might occur at the brace side of the weld. For this reason, the weld thickness on the brace side

becomes important. The outer and inner weld paths on the brace side are denoted as points B_o and B_i respectively as shown in Fig. 3.8. The distance $k'_3 t_b$ of projection of the inner weld path from the inner intersection curve on the brace side can be calculated from the inner dihedral angle γ_i as follows:

$$k'_3 = \begin{cases} \frac{k_3}{\cos \gamma_i}, & \gamma_i \leq \pi/4 \text{ or } \gamma_i \geq 3\pi/4 \\ \frac{k_3}{\cos(3\pi/4)} & \pi/4 \leq \gamma_i \leq 3\pi/4 \end{cases} \quad (3.50)$$

By inspection of the actual outer weld thickness on the brace side of some full scale specimens, it is seen that the outer weld thickness on the chord and brace sides are almost similar. Therefore, the distance $k'_2 t_b$ of projection of the outer weld path from the outer intersection curve on the brace side can be written as follows:

$$k'_2 = k_2 = \begin{cases} FOS_{\text{outer}} \left[1 - \left(\frac{\gamma_i - \theta_s}{\pi - \theta_s} \right)^{n_2} \right], & \gamma_o \leq 3\pi/4 \\ a\gamma_i + b - \frac{1}{\sin \gamma_i} & 3\pi/4 \leq \gamma_o \leq \pi \end{cases} \quad (3.51)$$

with parameters a and b defined in Eqn. (3.48). Verification of the model for outer weld path on the brace side is presented in Chapter 5. Unfortunately, the inner weld path cannot be verified since the full scale specimen could not be opened up by the current equipments.

3.6 GEOMETRY OF PARTIALLY OVERLAPPED CHS K-JOINTS

The geometrical model of a partially overlapped CHS K-joint on the outer surface involves three intersection curves, which intersect each other in sequence to form an intersection point as shown in Fig. 3.14. Furthermore, in the geometrical modelling of the weld, it is necessary to consider actual intersection points where the weld paths meet, and they are not the theoretical intersection point obtained by pure intersection of the CHSs. In this Section, the geometry of a partially overlapped CHS K-joint is discussed and an algorithm for the determination of the theoretical and actual intersection points is developed.

3.6.1 Partially overlapped CHS K-joint without welding details

As all equations presented in Sections 3.2 - 3.5 are for a single intersection curve, they should be appropriately transformed to be applied in the case of a partially overlapped K-joint. Fig. 3.14 shows a typical partially overlapped CHS K-joint. Fig. 3.15 shows the coordinate systems employed to defined the joint multiple intersection curves, in which the $O^{(1)}X^{(1)}Y^{(1)}Z^{(1)}$ coordinate system formed when the through brace intersects with the chord is considered as the global coordinate system. Other local coordinates are defined as follows:

- $O^{(2)}X^{(2)}Y^{(2)}Z^{(2)}$ - the local coordinate system of the chord, formed when the overlap brace intersects with the chord.
- $O^{(3)}X^{(3)}Y^{(3)}Z^{(3)}$ - the local coordinate system of the through brace formed when the overlap brace intersects with the through brace.
- $O^{(4)}X^{(4)}Y^{(4)}Z^{(4)}$ - the local coordinate system of the through brace, which has the same axes' directions as in $O^{(3)}X^{(3)}Y^{(3)}Z^{(3)}$, but the origin $O^{(4)}$ is placed at the intersection point between through brace's axis and the surface of the chord.

The relationships among these coordinate systems are sketched in Figs. 3.15b - 3.15c.

Assuming the projected length of overlap on the chord between the through brace and the overlap brace is g as shown in Fig. 3.15c, the projected diameter of the overlap brace on the chord is h and the two distances stated above are dimensioned in the direction of the global $Z^{(1)}$ axis. Other parameters defined the configuration of the joint are illustrated in Fig. 3.15 and named as:

- radius of the chord R_0 ,
- radius of the through brace R_1
- radius of the overlap brace R_2
- intersection angle between the through brace and the chord θ_1 , ($30^\circ \leq \theta_1 \leq 90^\circ$)
- intersection angle between the overlap brace and the chord θ_2 , ($30^\circ \leq \theta_2 \leq 90^\circ$)
- intersection angle between the overlap brace and the through brace $\theta = \pi - \theta_1 - \theta_2$.

The percentage of overlapping, p , is defined as:

$$p = \frac{g}{h} \times 100\% = \frac{g \sin \theta_2}{2R_2} \times 100\%. \quad (3.52)$$

In order to form a geometrical model for a partially overlapped CHS K-joint, the relationships among different coordinate systems should be established. The relationship between the coordinate system $O^{(1)}X^{(1)}Y^{(1)}Z^{(1)}$ and the coordinate system $O^{(2)}X^{(2)}Y^{(2)}Z^{(2)}$ can be expressed as:

$$\begin{cases} X^{(1)} = X^{(2)} \\ Y^{(1)} = Y^{(2)} \\ Z^{(1)} = -Z^{(2)} - d \end{cases} \quad (3.53)$$

d is the distance between the origins of the two coordinate systems. By inspection, the value of d can be established as:

$$d = \frac{R_1}{\sin \theta_1} + \frac{R_2}{\sin \theta_2} - g. \quad (3.54)$$

Similarly, consider the overlap brace and the through brace as another pair of CHSs forming a single intersection curve in the local coordinate system $O^{(3)}X^{(3)}Y^{(3)}Z^{(3)}$. Supposed that $O^{(4)}X^{(4)}Y^{(4)}Z^{(4)}$ is another local coordinate system of the through brace, of which the origin is defined in Fig. 3.15. By inspection, the distance q between $O^{(3)}$ and $O^{(4)}$ is written as:

$$q = O^{(3)}K - O^{(4)}K, \quad (3.55)$$

where:

$$O^{(4)}K = \frac{d}{\sin \theta} \sin \theta_2 \text{ and } O^{(3)}K = \frac{R_1}{\tan \theta}. \quad (3.56)$$

Hence, q yields:

$$q = \frac{d}{\sin \theta} \sin \theta_2 - \frac{R_1}{\tan \theta}. \quad (3.57)$$

The relationship between $O^{(3)}X^{(3)}Y^{(3)}Z^{(3)}$ and $O^{(4)}X^{(4)}Y^{(4)}Z^{(4)}$ therefore can be expressed as:

$$\begin{cases} X^{(4)} = X^{(3)} \\ Y^{(4)} = Y^{(3)} \\ Z^{(4)} = Z^{(3)} - q \end{cases} \quad (3.58)$$

The intersection curve between the overlap and through braces expressed in the local coordinate $O^{(4)}X^{(4)}Y^{(4)}Z^{(4)}$ can be transformed into the global $O^{(1)}X^{(1)}Y^{(1)}Z^{(1)}$ by applying the relationship described in Eqn. (3.3):

$$\begin{cases} X^{(1)} = X^{(4)} \cos \theta_1 + Z^{(4)} \sin \theta_1 + R_0 \\ Y^{(1)} = Y^{(4)} \\ Z^{(1)} = Z^{(4)} \cos \theta_1 - X^{(4)} \sin \theta_1 \end{cases}$$

3.6.2 Geometrical intersection point

As shown in Figs. 3.14 - 3.16, there is an intersection point where the three intersection curves meet. The intersection point can be found for each pair of intersection curves and subsequently it can be proved that these intersection points are coincided.

For a more flexible reference to the position of the intersection curves, the outer intersection between the chord and the through brace, the visible part, is named as intersection curve 1, the outer intersection between the chord and the overlap brace is named as intersection curve 2, the outer intersection between the through and overlap braces is named as intersection curve 3, and the hidden part of the intersection between the chord and the through brace is named as intersection curve 4. The names of the intersections are given in Fig. 3.16.

Consider the relative position between intersection curves 1 and 2 as shown in Fig. 3.17. Similar to the intersection described in Eqns. (3.9) and (3.11), the intersection curves of the through and overlap braces to the chord can be written in the $Y'Z'$ plane respectively as:

$$\begin{cases} Y^{(1)'} = R_0 \arcsin \frac{u}{R_0} \\ Z^{(1)'} = \left[v - R_0 \left(1 - \cos \frac{Y'}{R_0} \right) \cos \theta_1 \right] \frac{1}{\sin \theta_1} \end{cases} \quad (3.59)$$

$$\begin{cases} Y^{(2)'} = R_0 \arcsin \frac{a}{R_0} \\ Z^{(2)'} = - \left[b - R_0 \left(1 - \cos \frac{Y'}{R_0} \right) \cos \theta_2 \right] \frac{1}{\sin \theta_2} - d \end{cases} \quad (3.60)$$

with $u = R_1 \sin \alpha_1$, $v = R_1 \cos \alpha_1$,

$$a = R_2 \sin \alpha_2, b = R_2 \cos \alpha_2. \quad (3.61)$$

For the relative positions between the two curves, by inspection, there are normally 4 cases which can be described by the formulations below:

- Case (a): no intersection points $r_1 + r_2 < d$
- Case (b): one intersection point only $r_1 + r_2 = d$ (b1) or $r_1 - r_2 = d$ (b2)
- Case (c): two distinct intersection points $r_1 + r_2 > d$
- Case (d): center point O_2 moves from position (b2) toward O_1 , the joint is no more a partially overlapped CHS K-joint $r_1 - r_2 > d$

By calculating the abscissa of the left point U of curve 1 and that of the right point V from curve 2, r_1 and r_2 obtain their values as:

$$r_1 = \frac{R_1}{\sin \theta_1}; r_2 = \frac{R_2}{\sin \theta_2} \quad (3.62)$$

Consider the aforementioned three cases with the value of r_1, r_2 given in Eqn. (3.62) and of d given in Eqn. (3.54), we have:

- For case (a): $r_1 + r_2 < d \Leftrightarrow \frac{2R_2}{\sin \theta_2} \times \frac{p}{100} < 0 \Leftrightarrow p < 0$.
- For case (b): $r_1 + r_2 = d \Leftrightarrow \frac{2R_2}{\sin \theta_2} \times \frac{p}{100} = 0 \Leftrightarrow p = 0$.
or $r_1 - r_2 = d \Leftrightarrow \frac{2R_2}{\sin \theta_2} \times \left(1 - \frac{p}{100}\right) = 0 \Leftrightarrow p = 100$.
- For case (c): $r_1 + r_2 > d \Leftrightarrow \frac{2R_2}{\sin \theta_2} \times \frac{p}{100} > 0 \Leftrightarrow p > 0$.
- For case (d): $r_1 - r_2 > d \Leftrightarrow \frac{2R_2}{\sin \theta_2} \times \left(1 - \frac{p}{100}\right) < 0 \Leftrightarrow p > 100$.

The range of p for each case is summarized in Fig. 3.18. From the figure, it can be concluded that with $0 \leq p \leq 100$, which is often specified for a partially overlapped CHS K-joint, the presence of the intersection points can be ensured.

The algorithm for finding the solution of the intersection point is described in Figs. 3.19 and 3.20. It is observed that the driving angle α_1 at the intersection point cannot be less than the value of 90° due to the fact that $R_1 \geq R_2$. It is also observed that the

solution can be found when $\alpha_2 \in [\alpha_{2,\text{start}}, 180^0]$ and $\alpha_1 \in [\max(\alpha_{1,\text{start}}, 90^0), 180^0]$, whereas $\alpha_{2,\text{start}}$ is the angle at which $Z^{(1)}|_U = Z^{(2)}$, and vice versa for $\alpha_{1,\text{start}}$ (Fig. 3.19).

The algorithm can also be summarised in 4 steps as follows (with reference to Fig. 3.19):

- Step 1: Determine $\alpha_{2,\text{start}}, \alpha_{1,\text{start}}$, therefore determine the locations a, b, c, d .
- Step 2: Divide the arc $[\alpha_{2,\text{start}}, 180^0]$ into two equal parts base on the driving angle α_2 then get the respective coordinate $Y^{(2)}, Z^{(2)}$ of Point I.
- Step 3: Find in curve (1) the point having the same Z' value as that of Point I from curve (2). There are two points in curve (1) be able to satisfy this binding condition. The appropriate point, named as Point II, should has $\beta = \alpha_1 > c$.
- Step 4: Compare the values of Y' between Point I and Point II. If $Y^{(2)} < Y^{(1)}$, then the solution must be found in the remaining left parts to Point I and Point II of curves (1) and (2) respectively. Otherwise it must be determined in the right parts to Point I and Point II of curves (1) and (2) respectively. The final difference of coordinate value between Points I and II can be used to determine the solution:

$$e_{rr} = \frac{1}{2} \sqrt{(X^{(1)} - X^{(2)})^2 + (Y^{(1)} - Y^{(2)})^2 + (Z^{(1)} - Z^{(2)})^2}. \quad (3.63)$$

In this study, e_{rr} can be priorily set equals to $10^{-4}R$.

In general, the algorithm is able to converge after a few thousand iterations with a prescribed error equals to $10^{-4}R$, with R being the radius of the brace. With a computer having an internal memory of two gigabytes, the CPU time needed for the solution is less than one minute. Solution for the intersection point between curve (1) and curve (3) can be found in a similar manner.

Supposed that the first intersection point between intersection curves 1 and 2 is named as M, and the second intersection point between intersection curves 1 and 3 is named as N. The positions of M and N can be referred to in Fig. 3.21. Because N belongs to curve (3), which is the intersection curve between the through and the overlap braces, it must be positioned at the face of the overlap brace. Simultaneously, as N is the intersection between curve (3) and curve (1), it must lie on curve (1). However, the

only point that (1) has in common with the face of the overlap brace is M, which is the intersection point between intersection curves (1) and (2). Therefore, N must be coincided with M.

3.6.3 Range of some geometrical parameters

In general, the radii of the through brace R_1 and of the overlap brace R_2 as well as their intersection angles θ_1 and θ_2 are different. From Fig. 3.15, the distance between the projections of their axes on the chord is expressed as d and is the function of R_1 , R_2 , θ_1 , θ_2 and percentage of overlap p :

$$d = \frac{R_1}{\sin \theta_1} + \frac{R_2}{\sin \theta_2} - \frac{2R_2}{\sin \theta_2} \times \frac{p}{100}.$$

Partially overlapped CHS K-joints represent a heavy duty joint type. To ensure their fatigue properties, the percentage of overlap should satisfy some requirements. The DEn (1984) requires that, when the braces are overlapped, the amount of overlap should not be less than 20%. Almar (1985) reported that overlap joints have better fatigue performance than that of individual ordinary joints to 80% of overlap. Therefore, the amount of overlap in this project is limited from 20% to 80%. That means:

$$0.2 \leq p = \frac{\sin \theta_2}{2R_2} \left(\frac{R_1}{\sin \theta_1} + \frac{R_2}{\sin \theta_2} - d \right) \leq 0.8 \quad (3.64)$$

Normally, the intersection point of the axes of the through and the overlap brace does not lie on the axis of the chord, and the perpendicular distance between this intersection point and the chord axis is known as the eccentricity e of the joint. In all design code, there is a maximum limit on the value of e , which can be written as:

$$-1.1R_0 < e \leq 0.5R_0 \quad (3.65)$$

By inspection, s is calculated as:

$$e = s - R_0, \quad (3.66)$$

$$\text{where } s = \frac{d \sin \theta_1 \sin \theta_2}{\sin \theta} \quad (3.67)$$

Therefore:

$$-0.1 < \frac{d \sin \theta_1 \sin \theta_2}{R_0 \sin \theta} \leq 1.5. \quad (3.68)$$

with d calculated by Eqn. (3.54).

3.6.4 Partially overlapped CHS K-joints with welding details

Following the method to define a single welding profile in Section 3.5 and the geometry of a partially overlapped CHS K-joint without welding details in Section 3.6.1, the geometry for a welded partially overlapped CHS K-joint can be developed as shown in Fig. 3.22. Along each theoretical intersection curve, there are 4 other welding curves, denoted for example for theoretical intersection curve 1 as:

- Outer welding curve on the chord side, curve 1CO (point W_o in Fig. 3.8)
- Inner welding curve on the chord side, curve 1CI (point W_i in Fig. 3.8)
- Outer welding curve on the brace side, curve 1BO (point B_o in Fig. 3.8)
- Inner welding curve on the brace side, curve 1BI (point B_i in Fig. 3.8)

Due to the thickness of the weld, new intersection points are formed. Normally, there are 6 points, of which 3 points position on the outer surfaces of the CHSs, 3 other points position on the inner surface of the CHSs. The outer intersection points are shown in Fig. 3.23. The coordinates of the intersection points can be determined by the algorithm developed in Section 3.6.2 on the unfolded plane as described in Section 3.2. The pairs of intersection curves forming the actual intersection points are listed as follows:

- On the outer surface of the chord: curve 1CO $\cap$ curve 2CO to form point 1, curve 1CO $\cap$ curve 2CI to form point 4.
- On the outer surface of the through brace: curve 1BO $\cap$ curve 3CO to form point 2, curve 1BO $\cap$ curve 3CI to form point 5.
- On the outer surface of the overlap brace: curve 2BO $\cap$ curve 3BO to form point 3, curve 2BI $\cap$ curve 3BI to form point 6.

3.7 VERIFICATION OF THE GEOMETRICAL MODEL

3.7.1 Design and fabrication of specimens

Four small scale specimens are designed and fabricated such as their geometrical configuration fall within the application range for partially overlapped CHS K-joints as shown in Section 1.3. The small scale specimens are named from SS1 to SS4. Their measured dimensions are tabulated in Table 3.2. Specimen SS4 is specially designed such as the chord, the through and overlap brace have equal diameter. The fabrication procedure consists of 4 steps including profiling, welding, half cutting the specimens along their symmetrical axes and preparing spare CHSs. During the fabrication of the joints, the formation of the actual intersection points as described in Section 3.6.5 can be observed. After cutting all full small scale specimens along their symmetrical axis, the half specimens are renamed in pairs as SS1L, SS1R and so on.

3.7.2 Weld thickness measurement

In order to obtain as much information of the geometry of the weld profile in the small scale specimens as possible, a proper measurement technique should be established. For this purpose, soft clay is used to cover the weld for each half model. The clay mould with marked initiation point is pulled out when it is half-dried; by this method the welding profile would be imprinted on the clay. The weld information imprinted on the clay can be transformed onto a piece of paper by an etching method using a slave tube prepared previously.

The measurement process is illustrated in Fig. 3.24. Subsequently, the measurement can be digitalised by the drawing software AutoCAD and therefore measured weld thickness on the chord side can be plotted together with the theoretical outer and inner intersection profile was plotted in the same graph to form the basis of measurement. Modelled outer and inner weld curves are also plotted for comparison purpose. Unfortunately, the weld thickness on the brace side could not be captured since its importance was underestimated at the time the small scale specimens were fabricated and analysed.

3.7.3 Measurement results

For comparison purpose, measured and modelled outer and inner welds, as well as outer and inner intersection curves on the chord side are plotted in profile graphs for each half specimen. Besides, as k_{Tw} value characterizes the weld thickness, k_{Tw} graphs are used for the comparison of weld thickness between the measured and modelled small scale joints as well as the requirements from the API (2000) and AWS (1996). Such pictures and graphs for a typical half joint SS4R are presented in Figs. 3.25 and 3.26 respectively. The complete set of graphs for all small scale specimens is shown in Appendix B.

It can be observed that modelled joints resemble actual shapes both in the overall configuration, outer weld profile and position of the common area between welds. In addition, it can be seen that the measured outer weld paths are generally larger than the modelled profiles, especially at the crown toes and crown heels position. At the saddle position, for Specimen SS4R, the geometrical outer weld model overestimates the measured outer weld path as shown in Fig. 3.26. The biggest overestimation between the measured and modelled outer weld profiles is 23.5% compared to the brace thickness. In addition, from the respective k_{Tw} -graphs, it can be concluded that the measured weld thickness satisfies both the AWS and the API requirements for all joints. Furthermore, on the chord side, measured weld thickness is generally larger than modelled weld, which shows that the geometrical model would be conservative when it is used to predict the SCF or SIF values. The above conclusions are made based on the fact that the k_3 values for the inner weld path are relatively small compared to the k_2 value for the outer weld path. Although the outer weld profiles showed reasonably good compatibility to the specifications and the geometrical model, the inner weld profiles could hardly achieved the requirement for full penetration and lack of weld material is often observed in the position of crown heels.

Weld thickness on the chord side is a combination of outer and inner weld paths. Generally, as shown in Fig. 3.26 and Appendix B, measured weld thickness is larger than modelled thickness and hence, satisfied the requirements the codes. It is therefore proved that the geometrical model is conservative and safe for subsequent analysis.

3.7.4 Validation of parameters in the geometrical model

The inner weld path model is intended to account for the cut-off effect of the weld thickness when the dihedral angle is less than 90° . Wong (2000) has shown a distance of T_3 to shift in (cut inside) the point A_i to the point W_i when γ_i is between 30° and 90° . When rises to 90° , $k_3 = 0$ (no alteration is required). k_3 will turn to a surplus value (fill) as γ_i greater than 90° . To examine the compatibility of the physical joint with the weld root model, the value of k_3 measured and k_3 according to Wong (2000) model for Specimen SS4R is plotted against the dihedral angle γ in Fig. 3.27. In this figure, region where Wong's model obtains conservative values is the area below the original curve and the unconservative region is otherwise defined. From the figures, it can be seen that measured weld root values often far exceed the values calculated by Wong's model, meaning that lack of weld penetration in the actual model is more severe than predicted. Based on the observation, amendment to the inner weld path model is made accordingly such as a larger value of $F_{OSinner}$ of 0.6 is proposed instead of the existing factor of 0.25.

Therefore, the expression for k_3 is modified as:

$$k_3 = F_{OSinner} \left[1 - \left(\frac{\gamma_i - \theta_s}{\pi / 2 - \theta_s} \right)^{n_3} \right] \quad (3.69)$$

with $F_{OSinner} = 0.6$ and other parameters keep unchanged. The inner weld path with validated parameters as (3.69) is plotted in Fig. 3.27.

Adjustment in k_3 expression results in smaller weld thickness whose satisfaction to the codes of practice is questionable. In addition, it is mentioned in Section 3.7.3 that the geometrical model of the weld thickness on the chord side is over-predicted in the saddle region. The outer weld path model, is therefore, also need adjustment. For the former purpose, $F_{OSouter}$ is recalibrated by forcing the AWS requirement at $\gamma_o = \gamma_i = 90^\circ$ in the outer and inner weld paths on the chord side. $F_{OSouter}$ is determined as 0.55 instead of the existing value of 0.3. Furthermore, the starting point for the outer weld path to change its direction is reduced to 108° instead of 135° . The outer weld path on the chord side can be rewritten as follows:

$$k_2 = \begin{cases} F_{OS_{outer}} \left[1 - \left(\frac{\gamma_o - \theta_s}{\pi - \theta_s} \right)^{n_2} \right], & \gamma_o \leq 0.60\pi \\ a\gamma_i + b - \frac{1}{\sin \gamma_i} & 0.60\pi \leq \gamma_o \leq \pi \end{cases} \quad (3.70)$$

in which:

$$\begin{aligned} a &= \frac{\cos \gamma_i}{\sin^2 \gamma_i} \Big|_{\gamma_o=0.60\pi} - \frac{n_2 F_{OS_{outer}}}{0.60\pi - \theta_s} \left(\frac{0.60\pi - \theta_s}{\pi - \theta_s} \right)^{n_2-1} \\ b &= F_{OS_{outer}} \left[1 - \left(\frac{0.60\pi - \theta_s}{\pi - \theta_s} \right)^{n_2} \right] + \frac{1}{\sin \gamma_i} \Big|_{\gamma_o=0.60\pi} - a\gamma_i \Big|_{\gamma_o=0.60\pi} \end{aligned} \quad (3.71)$$

Parameters of the weld model are summarized as follows:

$$\begin{cases} F_{OS_{outer}} = 0.55, & F_{OS_{inner}} = 0.6 \\ n_2 = 2.0, & n_3 = 0.4 \end{cases} \quad (3.72)$$

The effect of adjustment to the profile graph and k -graph is plotted in Figs. 3.28 and 3.29 accordingly. It is seen that the validated weld thickness is satisfactory for the requirements of the AWS (1996) and the API (2000). As seen in the figures, the validated weld thickness model is reduced at saddle position but slightly increased at others to maintain the continuity of outer and inner weld paths. It is seen that the validated geometrical model for the weld on the chord side is able to cater for the uncertainties observed in the measurement of weld profiles for small scale specimens: the cut-off effect is clearer; and the weld thickness in the saddle position is reduced. Therefore, the validated model weld thickness on the chord side can be in the subsequent mesh generation and numerical analysis; with the inner path described by Eqn. (3.69) and outer path denoted by Eqns. (3.70) and (3.71) associated with supplementary parameters specified in Eqn. (3.72). In addition, the geometrical model of the weld profile will be compared with the measured weld thickness for two full scale test specimens of partially overlapped CHS K-joints as presented in Section 5.2.3

3.8 CRACK MODEL FOR PARTIALLY OVERLAPPED CHS K-JOINTS

3.8.1 Crack surface modelling

Fatigue test results have shown that the crack not only curves around the joint

intersection following the weld paths (Huang 2003, Shao 2005, Ji 2007). It also curves through the CHS wall under the weld path and along its own length either on the chord or brace sides. However, in three-dimensional FE modelling, it is complicated to define the curved crack surface under the weld path. Therefore, for simplicity, many researches have been conducted with the assumption that the crack surface is perpendicular to the weld path (Bowness and Lee 1995, Lie et al. 2003).

For comparison purpose, the crack surface in this study is modelled such as at a certain section along the crack length and perpendicular to the wall of the CHS, the crack surface makes an angle $\omega(s')$ to the perpendicular line as shown in Fig. 3.30. In detail, a local coordinate system $x'-y'-s'$ is defined with origin at a certain point W_0 along the outer weld path in such a way that the y' and the s' axes are, respectively, perpendicular and tangential to the weld path at W_0 . The axis x' is defined such as it is parallel to the global axis X . In addition, D is defined as the depth of the crack at section W_0 along the crack length such that a' and a are, respectively, the curved and projected depths of W_0D on the vertical line. The angle between the global Z and local s' axes is equal to $\phi(s')$.

The coordinates of a point D lying on the crack surface can be determined according to their relative position to the local origin W_0 based on the crack surface angle ω as:

$$\begin{bmatrix} X \\ Y \\ Z \end{bmatrix} = \begin{bmatrix} 1 & 0 & 0 \\ 0 & \cos \phi(s') & \sin \phi(s') \\ 0 & -\sin \phi(s') & \cos \phi(s') \end{bmatrix} \begin{bmatrix} x' \\ y' \\ s' \end{bmatrix} + \begin{bmatrix} a' \sin(\omega(s')) \\ -a' \cos(\omega(s')) \\ 0 \end{bmatrix}. \quad (3.73)$$

3.8.2 Crack front modelling

Actual measurement of crack front done after the fatigue test (Lee et al. 2007a) indicated that the crack front resembles an elliptical curve on a projected flat plane when the crack surface is opened up. However, as the crack length might not be symmetrical on the two sides of the reference point for a general load combination, which includes all AX, IPB and OPB loads, the shape of the crack front in this study is modelled as a unsymmetrical bi-elliptical curve on the projected $s'-t'$ plane as shown in Fig. 3.31 and Eqns. (3.74a) and (3.74b):

$$\text{For } l_{cr1} \leq s' \leq l_{cr0} : \quad \frac{(s' + l_{cr0})^2}{(l_{cr1} + l_{cr0})^2} + \frac{(t')^2}{(a')^2} = 1 \quad (3.74a)$$

$$\text{For } l_{cr0} \leq s' \leq l_{cr2} : \quad \frac{(s' + l_{cr0})^2}{(l_{cr2} + l_{cr0})^2} + \frac{(t')^2}{(a')^2} = 1 \quad (3.74b)$$

In Eqns. (3.74a) and (3.74b), l_{cr1} , l_{cr2} and l_{cr0} are the corresponding position along the s' axis of the crack tip 1, crack tip 2 and the deepest point. Obviously, the bi-elliptical attends a maximum value of a' when $s' = -l_{cr0}$ and becomes a single elliptical curve when $l_{cr1} = l_{cr2}$ and $l_{cr0} = 0$. Furthermore, the points on the crack front can be generated once the values of l_{cr1} , l_{cr2} , l_{cr0} and a' are provided from the actual measurement on tested specimen once the crack is opened up. The position of the crack front situated on the crack surface is given in Fig. 3.32. The validation of the crack surface and crack front model is given in Section 5.6 where measurement of the opened crack is available.

3.9 CONCLUDING REMARKS

In this Chapter, firstly the geometrical modelling of the theoretical and weld path for a single intersection curve is presented. In the theoretical modelling of a single intersection curve, a supplementary formula for the reverse mapping of a plane into a CHS and an amendment to the formula to calculate the angle β are presented. In the weld model, the application range is extended for the case when the chord and brace are of equal diameter. Additional modelling of the weld thickness on the brace side is also presented. Secondly, the theoretical and weld model are extended for the case of multiple intersections such as for partially overlapped K-joints. Due to the geometry of the joints studied, an algorithm is developed for the determination of the theoretical and actual intersection points between theoretical intersection curves, and between several weld paths. Thirdly, the proposed geometrical model of the weld on the chord side is validated by actual measurement on some small scale partially overlapped CHS K-joints. Parameters in the weld model are then adjusted. Subsequently, a model for the crack surface and crack front is proposed for the modelling of partially overlapped CHS K-joints.

Fig. 3. 1 Co-ordinate systems for the determination of a singular intersection curve

Fig. 3. 2 Mapping a plane to a CHS surface

(b) Transformation of curve Ω using Eqn. (3.7)

(c) Transformation of curve Ω using Eqn. (3.8)

Fig. 3. 3 Reverse transformation of a circular tube into a plane

Fig. 3. 4 Double mapping a circle to an intersecting curve (Plan view)

Fig. 3. 5 Geometry of the dihedral angle along the joint intersection

Fig. 3. 6 Geometry and definition of angle β_a in the YZ plane

Fig. 3. 7 Inner and outer intersecting curves with weld path

(a) Modelling of welded joint ($30^\circ \leq \gamma < 90^\circ$) – Section 1 – 1

(b) Modelling of welded joint ($90^\circ \leq \gamma < 180^\circ$) – Section 2 – 2

Fig. 3. 8 Modelling of the welded joint

Fig. 3. 9 Geometrical models for the outer weld path on the chord side

Fig. 3. 10 Original contact thickness

Fig. 3. 11 Behaviour of inner and outer dihedral angle curves with different ratios of r/R

Fig. 3. 12 Modelling of the outer intersection and weld path for the case $r/R = 1.0$

Fig. 3. 13 Comparison of welding thickness for the case $r/R = 1.0$ ($\theta = 90^\circ$)

Fig. 3. 14 A typical partially overlapped CHS K-joint (half model)

(a) Overall view

(b) Distance between coordinate systems

(c) Percentage of overlap

Fig. 3. 15 Relationships of coordinate systems

Fig. 3. 16 Geometrical intersection curves (half model)

Fig. 3. 17 Relative position of intersection curves 1 and 2

Fig. 3. 18 Range of p for the relative position of intersection curves 1 and 2

Fig. 3. 19 Solution for the intersection point between intersection curves 1 and 2

Fig. 3. 20 Flow chart for finding intersection point of curves 1 and 2

Fig. 3. 21 Solution for the intersection point of the 3 curves

Fig. 3. 22 A typical welded partially overlapped CHS K-joint

Fig. 3. 23 Close-up of the common area between three welded CHS

(a) Soft clay covered around weld

(c) Half dried clay detached

(c) Weld profile printed on clay

(d) Weld profile traced on paper

Fig. 3. 24 Measurement procedure

Fig. 3. 25 Profile graph for welding along intersection curve 1, Specimen SS4R

Fig. 3. 26 k_{Tw} -graph for welding along intersection curve 1, Specimen SS4R

Fig. 3. 27 Modelled and measured inner weld paths for Specimen SS4R

Fig. 3. 28 Effect of validated parameters on profile for curve 1/Specimen SS4R

Fig. 3. 29 Effect of validated parameters on k_{TW} for curve 1/Specimen SS4R

Fig. 3. 30 Geometrical model for the crack surface

Fig. 3. 31 Geometrical model for the crack front

Fig. 3. 32 Overall view of the crack front situated on the crack surface

Dihedral Angle, γ	Minimum k_{API}	Minimum k_{AWS}
50°-135°	1.25	$\frac{1}{\sin \gamma}$
35°-50°	1.5	$\frac{1}{\sin \gamma}$
Below 35°	1.75	2.0 (for $\gamma < 30^\circ$)
Over 135°	Build out to full thickness but need not exceed 1.75	Build out to full thickness but need not exceed 1.75

Table 3. 1 Summary of k_{API} and k_{AWS} requirement for weld thickness

	D_0	D_1	D_2	t_0	t_1	t_2	θ_1	θ_2	p
SS1	139.68	113.02	87.07	12.83	11.93	9.68	58.25	59.87	0.73
SS2	113.02	99.53	87.10	11.80	11.02	9.35	43.75	44.25	0.47
SS3	136.00	113.33	99.67	11.63	11.83	11.13	59.5	43.25	0.41
SS4	113.00	113.00	113.00	11.80	11.82	11.80	45.25	60.00	0.46

Table 3. 2 Measured dimensions for small scale specimens

CHAPTER 4

MESH GENERATION FOR PARTIALLY OVERLAPPED CHS K-JOINTS

4.1 INTRODUCTION

Mesh generation is one of the key parts in a FE analysis. The quality of the mesh affects not only the accuracy of the results but also the efficiency of the analysis, especially for problems involving high stress concentration in small regions. For the purpose of generating meshes for tubular joints, either multi-purpose commercial software packages such as ABAQUS, ANSYS and MARC or specially designed pre-processors such as PRETUBE and PATRAN have been utilized. However, Bowness and Lee (1995a) found that such packages are unable to ensure the quality of joints with welds and cracks. The problem becomes worse when the diameter ratio between the brace and the chord of the joint is greater than 0.8. In addition, their effectiveness in handling a great number of meshes due to the variation of geometrical parameters is still questioned.

Several factors should be considered in the process of mesh generation, for example element type, the number of element layers through the thickness and mesh refinement. Herion et al. (1996) evaluated the efficiency of different element types in modelling tubular joints and emphasized that 3D quadratic solid element is most suitable, especially for joints with welding details. For the generation of solid mesh for tubular joints without cracks, systematic methods were pioneered by Cao et al. (1997). This method has been further developed for the modelling of weld details and the cracks in T/Y and gap K-joints by Lee et al. (1999) and Lie et al. (2003). Following this method and based on the geometrical model discussed in Chapter 3, a mesh generator is developed for partially overlapped CHS K-joints with and without cracks. The mesh generator is constructed step-by-step, therefore it is able to produce several kinds of meshes such as surface meshes, solid meshes, meshes with or without welding and

crack details. At each step, a particular mesh can be exported depending on the nature of the problem under consideration.

4.2 REQUIREMENT OF THE MESH GENERATION SCHEME

Requirements should be set to ensure the quality of the generated mesh. For a reasonable degree of accuracy and efficiency of the FE analysis, it is necessary that the generated meshes should satisfy the following requirements:

- The mesh should be refined in regions where high stress gradient exists for good stress prediction while it could be coarser in regions far away from high stress gradient positions in order to save computational cost. In addition, the mesh should be symmetric whenever the structure of the model is symmetric.
- The mesh generator should be able to automatically adjust the shape of elements where badly distortion is identified. Further from the intersection, slightly distorted elements are acceptable.
- The mesh generator should be able to detect any unreasonable input parameters.

4.3 METHOD OF MESH GENERATION

The existence of several intersection curves and intersection points makes the mesh generation of a partially overlapped CHS K-joint not a trivial task. To reduce the order of complexity, the mesh of a partially overlapped CHS K-joint is first created in the form of a surface mesh, which is then converted into a solid mesh using an extrusion algorithm (Xu 2006). This method of mesh generation is flexible in such a way that at different modelling stages, meshes with different levels of details can be generated and employed for special applications. For example, a surface mesh without any welding details (SURF_0W) could be extracted out for quick assessment of the fatigue strength of the joint while a surface mesh with welding details (HYBR_1W) could be formed with little addition effort for quick SCF estimation. For a more intensive and detailed study on the distribution of HSS around the joint intersection, full solid meshes (SOLID_1W and SOLID_0W) with or without welding details could be used. Eventually, in the case that a full numerical study for a crack joint is needed, a full

solid mesh with welding and crack details (SOLID_CR) can be generated. In these meshes, different kinds of element are used depending on the targeted purpose. A flow chart for the mesh generation scheme is presented in Fig. 4.1.

As in each region, different geometry characterises and requires different mesh qualities, the whole surface mesh is divided into several zones. This method of mesh generation follows the philosophy of Bowness and Lee (1995a), Wong (2000) and Lie et al. (2003). After the mesh of every zone is successfully obtained, they are merged together to form the overall mesh. Furthermore, in order to improve the quality of the mesh, at first linear elements are adopted. After the mesh of the whole structure has been completed, mid-points of elements are inserted to transform linear elements into quadratic elements. By using this method together with an adaptive surface mesh generator (Lee 1999), the mesh near the joint intersection is able to satisfy the requirement of denser mesh for regions with high stress gradient. Another advantage is that the mesh can be refined in one zone without affecting other zones. By dividing a complex mesh generation into several steps, this approach to the development of a basic surface mesh is flexible and can save much time and effort in case any alteration is required.

For the generation of the solid mesh, the element size should be controlled in both thickness and surface directions. Firstly, in the thickness direction, the element size might be determined through the number of element layers through the thickness of the chord or braces. It is because after a study by Dyer (1996) on the effect of mesh density on the numerical analysis of the ultimate strength and SIF for tubular joints, it is advised that a fine mesh with at least three layers of element across the thickness and along the intersection should be used to cater for the stress concentration effect along the weld. For the partially overlapped CHS K-joints investigated, the thickness of the chord and brace members is divided into four layers within a radius of three times the weld thickness along the intersection. Further from such location, it is divided into two layers. Secondly, the element size in the surface direction along the intersection is determined to be equal to the weld thickness and will be varied following a linear rule such as it is coarser further from the intersection. It is noted that the element size in the surface direction is determined when the surface meshes are generated and the number

of element and position of nodes on the chord and brace side along an intersection is calculated such that they are equal compatible. It is to facilitate the element connectivity for the insertion of welding and crack details in subsequent steps.

4.4 SURFACE MESH GENERATION

As mentioned in Chapter 3, the weld path is not always projected outside the theoretical intersection curve. For cases with β ratio close to 1.0, the path on the chord side of a weld crosses the theoretical intersection curve. For this reason, extrusion of the middle surface mesh in the positive and negative normal surface directions shall lead to complicate solutions for the case $\beta = 1.0$. Hence, the extrusion procedure is started from the exterior boundary surface to facilitate the determination of the outer weld paths as illustrated in Fig. 4.2. For the automatic mesh generation of tubular joints for fatigue study, this approach where the surface mesh can be developed into solid mesh in such extrusion procedure is firstly adopted in this study. Therefore, the outer boundary surface is generated first to form a reference to other surfaces. For the generation of the outer boundary mesh, the entire surface is divided in several zones. As the percentage of overlap varies from 20% to 100% as specified in EuroCode 3 (2004) and the CIDECT Guide (Zhao et al. 2000), the shape of each zone does not follow any predetermined pattern. The use of a structured and tailor mesh, therefore, is impossible. For this reason, an adaptive mesh generator (Lee 1999) is applied to discretize these zones into surface meshes. The zones are shown in Fig. 4.3 and concise descriptions for them are given below:

- Zone A1: This zone lies inside the intersection curve formed when the through brace intersects with the chord.
- Zone A2: This zone lies inside the intersection curve formed when the overlap brace intersects with the chord.
- Zone A3: This zone lies inside the intersection curve formed when the overlap brace intersects with the through brace.
- Zone ThruB: This zone belongs to the through brace surface, connects it with zones A1 and A2. Zone ThruB can be extended to the total length of the through brace.
- Zone LapB: This zone resembles zone ThruB. In general, this zones is different

from zone ThruB. It is because the joint configuration is not symmetric when the angles between the braces and the chord are different.

- Zone E: This zone belongs to the outer side of the chord surface, and is able to be extended to the full length of the chord.

In the generation of the surface mesh, triangular elements are used. For positions where the weld imposes on the surface mesh, quadrilateral elements are used to facilitate node connection. After the outer surface mesh is generated, it is scaled down to fit the middle surface with or without welding details for HSS calculation (mesh SURF_0W or HYBR_1W) as illustrated in Figs. 4.4 and 4.5 respectively. Exact theoretical intersection curves are involved in the case of mesh SURF_0W, where calculated weld thickness along the intersections are involved in mesh HYBR_1W. For mesh HYBR_1W, at position of the weld paths, shell elements are replaced by solid elements as shown in Fig. 4.6.

Besides being used to obtain the middle surface mesh used for the generation of meshes SURF_0W and HYBR_1W, the exterior surface mesh developed in this step can also be used to continue generating other interior surface meshes, such as the boundary surface meshes inside the chord, the through and overlap braces. In any case, it is necessary that the middle surface mesh be generated, since it acts as the connector between the outer and inner boundary surface meshes.

4.5 SOLID MESH GENERATION

The outer surface mesh can be converted into solid mesh by connecting corresponding nodes on respective boundary surfaces using an algorithm to relate the nodal connectivity (Xu 2006). A typical extruded mesh without any welding details, named as mesh SOLID_0W, is shown in Fig. 4.7. For the implementation of this algorithm, the entire joint is divided into four spaces namely, the outer space (Space 1) and the inner space (Space 2) for the chord, the through brace (Space 3) and the overlap brace (Space 4) as shown in Fig. 4.8. In each space, the boundary surface mesh should be generated. The boundary surface for Space 1, which is the exterior surface of the three CHSs and identical to the outer surface mesh, is created first. Following that, the boundary surfaces for Spaces 3, 4 and 2 which are the interior surfaces of the through

brace, overlap brace and chord respectively, are created subsequently. The mid-surface mesh which connects to all other surfaces together is generated afterward.

The extrusion procedure also controls the number of layers of element and the level of adaptive refinement along the normal direction of the surface. In details, four layers of element are used across the thickness along the intersection both on the brace and chord sides of the weld. Fig. 4.8 shows the distribution of thickness layers for mesh SOLID_0W. To maintain the connectivity among all the elements generated, the final 3D mesh generated consists of four types of solid element, namely tetrahedron, prism, pyramid and hexahedron. The shape of these elements is shown in Table 4.1.

4.6 WELD PROFILE INSERTION

In fabrication, each CHS in the partially overlapped CHS is connected to the other by welding material, the same concept is adopted in the mesh generation scheme such that the solid mesh representing each CHS is connected to each other by weld strips. With that perception, the weld profile can be conveniently added into the extruded solid mesh since the images of the weld on the brace and chord sides are catered for when a basic outer surface mesh is generated. Because the connection strips between different CHS consist of quadrilateral shell elements, 3D prism elements are used to form the solid weld path. Figs. 4.9 and 4.10 show the solid mesh with welding details for exterior and interior views. There are four weld lines listed as follows:

- Weld 1 connecting the through brace and the chord in Space 1
- Weld 2 connecting the overlap brace and the chord in Space 1
- Weld 3 connecting the overlap brace and the through brace in Space 1
- Weld 4 connecting the overlap and through brace in Space 4

It is noted that actual intersection points as described in Section 3.6.4 are included as shown in Fig. 4.11; the space among them is filled up by tetrahedron elements as shown in Fig. 4.12.

4.7 CRACK PROFILE INSERTION

4.7.1 Crack tip position and crack zone extraction

It has been observed (Lee et al. 2007a) that for partially overlapped CHS K-joints, the fatigue crack occurred and propagated not only through the chord's wall as normally seen in other types of tubular joints. Depending on the configuration of the joint and the proportion of loading, it is possible that a surface crack can be formed at the brace side of the weld. In practice, during fatigue assessment, a surface (HYBR_1W) or solid mesh (SOLID_1W) with welding details would be created first to study the SCF along the joint to identify the location of the peak HSS which determines on which sides a surface crack shall be formed. Once the location of a surface crack is known, the extent of the crack, which is defined implicitly by the parameters l_{cr1} and l_{cr2} , could be determined by specifying their positions relative to the three reference points as shown in Fig. 4.13 and expressed in Eqn. (4.1):

$$l_{cr} = \int_{\alpha_{ref}}^{\alpha_{cr}} \left| \frac{dr}{d\alpha} \right| d\alpha = \int_{\alpha_{ref}}^{\alpha_{cr}} \left\{ \left(\frac{du}{d\alpha} \right)^2 + \left(\frac{dv}{d\alpha} \right)^2 \right\} d\alpha \quad (4.1)$$

In Eqn. (4.1), parameters r , u and v are normalised coordinates of the crack on the surface of the respective CHS, α is the driving angle, α_{ref} and α_{cr} are the driving angle at positions of the reference point and crack tip respectively. Illustration of the normalised coordinates is shown in Fig. 4.14. Once the value of crack length l_{cr} is predetermined, Eqn (4.1) becomes a procedure where binary search can be used to calculate the normalised coordinates of the crack tips through α_{cr} . In addition, as it has been observed in the experimental study (Lee et al. 2007a), the crack surface is not always perpendicular to the wall of the CHS. The inclined angle ω that the crack surface makes to the normal direction of the surface of the CHS as shown in Fig. 4.15, therefore, should also be specified.

The position of the crack tips, or the length of the crack in other words, determines the number of elements to be extracted from mesh SOLID_1W. These elements can be determined by an algorithm developed to search for elements inside and adjacent to the crack range. The detail of this algorithm in the form of a flow chart is shown in Fig. 4.16. As the aspect ratio of the elements around the crack tips concerns, the algorithm

is able to move the nodes of the element housing the crack tip such that the best aspect ratio for the element around the crack tips can be achieved. As the connectivity of the mesh in the surface crack block concerns, the algorithm is also able to substitute prism elements with hexahedral elements in the position where the crack block situates. However, the boundary nodes of the extracted zone are kept unchanged in order to re-merge the extracted zone to form the final solid mesh with crack, mesh SOLID_CR. For a partially overlapped CHS K-joint, the mesh SOLID_1W after extracting the crack zone in the brace side of Weld 1 is shown in Fig. 4.17.

4.7.2 Mesh generation for surface crack block

Following a definition of the crack front in Section 3.8, the coordinates of all nodes along the crack front can be calculated and mapped from their local u - v - w coordinate system to the local x - y - z coordinate system:

$$\begin{bmatrix} x \\ y \\ z \end{bmatrix} = \begin{bmatrix} l_1 & l_2 & l_3 \\ m_1 & m_2 & m_3 \\ n_1 & n_2 & n_3 \end{bmatrix} \begin{bmatrix} u \\ v \\ w \end{bmatrix} \quad (4.2)$$

It is noted that the local x - y - z coordinate system is parallel to the global X - Y - Z coordinate system, and l_i , m_i , n_i are the direction cosines of the u - v - w to the x - y - z coordinate system as shown in Fig. 4.18. The values of l_i , m_i , n_i at a point $D(X_D, Y_D, Z_D)$ can be calculated from the geometrical description of the crack front. For a sufficiently refined mesh near the crack front, the direction of vector $\mathbf{P}_1\mathbf{D}$ or $\mathbf{D}\mathbf{P}_2$ can be approximated as the direction of vector $\mathbf{w}$ as shown in Fig. 4.19. Therefore, the vectors l_3 , m_3 , n_3 can be calculated as:

$$l_3 = \frac{X_{P1} - X_D}{d_D}, \quad m_3 = \frac{Y_{P1} - Y_D}{d_D}, \quad n_3 = \frac{Z_{P1} - Z_D}{d_D} \quad (4.3)$$

where d_D is the distance between the two points:

$$d_D = \sqrt{(X_{P1} - X_D)^2 + (Y_{P1} - Y_D)^2 + (Z_{P1} - Z_D)^2} \quad (4.4)$$

Supposed that for each point D on the crack front, there is a corresponding point W_0 on the weld path. Point W_0 can be determined by the intersection between the crack

surface, the plane normal to it at point D, and the weld path as previously defined in Fig. 3.30. The tangent vector of the weld path at point W_0 is named as $\mathbf{s}$. By definition, unit vector $\mathbf{v}$ is perpendicular to the crack surface, therefore vector $\mathbf{v}$ is perpendicular to the vector $\mathbf{s}$. Vector $\mathbf{v}$ is also perpendicular to vector $\mathbf{W_0D}$. Therefore, vector $\mathbf{v}$ can be calculated:

$$\begin{aligned}\mathbf{v} = \mathbf{W_0D} \times \mathbf{s} &= \begin{vmatrix} \mathbf{i} & \mathbf{j} & \mathbf{k} \\ 0 & \sin \varphi & \cos \varphi \\ \cos \omega & -\sin \omega \cos \varphi & \sin \omega \sin \varphi \end{vmatrix} \\ &= \sin \omega \mathbf{i} + \cos \omega \cos \varphi \mathbf{j} - \cos \omega \sin \varphi \mathbf{k}\end{aligned}\quad (4.5)$$

where ω is the angle between the crack surface and the x -axis, and φ is the angle between the tangent direction and the z -axis, the direction cosines of $\mathbf{s}$ and $\mathbf{W_0D}$ are $\sin \varphi \mathbf{j} + \cos \varphi \mathbf{k}$ and $\cos \omega \mathbf{i} - \sin \omega \cos \varphi \mathbf{j} + \sin \omega \sin \varphi \mathbf{k}$, respectively.

The direction cosines for the unit vector $\mathbf{v}$ are therefore:

$$l_2 = \sin \varphi, \quad m_2 = \cos \omega \cos \varphi, \quad n_2 = -\cos \omega \sin \varphi \quad (4.6)$$

Similarly,

$$\begin{aligned}\mathbf{u} = \mathbf{v} \times \mathbf{w} &= \begin{vmatrix} \mathbf{i} & \mathbf{j} & \mathbf{k} \\ l_2 & m_2 & n_2 \\ l_3 & m_3 & n_3 \end{vmatrix} \\ &= (m_2 n_3 - m_3 n_2) \mathbf{i} - (l_2 n_3 - l_3 n_2) \mathbf{j} + (l_2 m_3 - l_3 m_2) \mathbf{k}\end{aligned}\quad (4.7)$$

Hence,

$$l_1 = m_2 n_3 - m_3 n_2, \quad m_1 = l_2 n_3 - l_3 n_2, \quad n_1 = l_2 m_3 - l_3 m_2 \quad (4.8)$$

Eqns. 4.3, 4.6 and 4.8 determines all terms in the transformation matrix (4.2). From the local coordinate of all nodes, the global coordinate can be conveniently obtained:

$$X = x + X_D, \quad Y = y + Y_D, \quad Z = z + Z_D \quad (4.9)$$

The mesh around the crack front is tailor-designed as a spider web consisting of concentric rings of four sided elements as shown in Fig. 4.19. The inner most ring of elements is degenerated to a ring of crack elements as shown in Fig. 4.20. This type of mesh design was found to be the most efficient for most crack problems (Yang 1999).

4.7.3 Element types in the surface crack block

It is well known that the stress field along the crack front is singular of order $-1/2$ (Irwin 1957) and a special crack element having singular finite element shape function has been created by Barsoum (1976). In this research, the similar three-dimensional quadratic prism with four midside nodes at the quarter point as shown in Fig. 4.23 for the mesh around the crack front. Prism elements are used as the transition element between the crack front and the field far away from the crack. Tetrahedron elements are adopted to link the crack elements and the adjacent elements. Pyramid elements are used to fill the void of different elements linked together in the crack region. A summary of the element types used in the surface crack block is given in Table 4.2. The entire crack block CRBLOCK is shown in Fig. 4.21.

4.7.4 Mesh generation for transition zones

Several mesh blocks have been generated for the smooth transition from the surface crack block to the extracted solid mesh. Since the initial solid mesh is developed from an unstructured surface mesh and the boundary nodes of the deleted zone cannot be changed while the surface crack block is a complex structured mesh, additional nodes have to be added in order to maintain the feasibility of the surface crack block generation. New element connectivity has to be created to ensure that the transition block can be generated successfully. Fig. 4.22 shows the mesh block TRICNN connecting mesh CRBLOCK to the extracted solid mesh in the direction towards Space 1. In the same figure, mesh block QUACNN shows the same connection except that it is in the direction towards Space 3. Another mesh QUAFIL is used to fill in the void between mesh blocks QUACNN and CRBLOCK. In addition, as the crack grows, the depth of the crack spreads from the outer surface of the weld through the thickness of the wall of the CHS. Since the solid mesh is initially designed with four layers of elements, the first layer of element in the surface crack block position is shifted down to full thickness of CHS. Generally, the transition meshes TRICNN, QUACNN and QUAFIL are designed to bring back the even distribution of elements through the thickness of the CHS.

Due to the mesh design in the block housing the two crack tips, their side faces have been discretized into several triangles. Therefore mesh blocks DCUBE-Q and DCUBE-T are introduced to modify these side faces connecting mesh CRBLOCK to mesh blocks QUACNN and TRICNN respectively. Mesh blocks DCUBE-Q and DCUBE-T consist of tetrahedral and pyramid elements in order to connect mesh CRBLOCK to other meshed as shown in Fig. 4.22. Furthermore, prism elements along the weld have to be converted into hexahedral elements for the generation of the surface crack block. That is the main reason for mesh block QUAFIL to be pasted between mesh blocks QUACNN and CRBLOCK. With the same reason, as shown in Fig. 4.22, mesh block HEPR is inserted to the two ends of the mesh CRBLOCK connecting itself to the extracted solid mesh.

4.7.5 Examples

The generation of the solid mesh with crack detail for a partially overlapped CHS K-joint, mesh SOLID_CR, is consisted of several steps. The preliminary Step 1 includes a generalized procedure for the positioning of the crack tips, followed by Step 2 with a versatile criterion for the extraction of nodes and elements in the area where the crack propagates. Step 3 involves the generation of a tailor-made crack block designed to incorporate the special purpose crack element. Step 4 generates several transition meshes to ensure a smooth transition from the crack block to the extracted solid mesh. The final Step 5 merges all individual meshes. Following the aforementioned steps, the surface crack is able to be located anywhere along one of four weld paths and propagated either on the brace or chord side of the weld. Examples of the crack located along Weld 1 on the chord and brace sides, and along Weld 3 on the chord side of the weld are given in Figs. 4.23, 4.24 and 4.25 respectively. Other possible locations of the weld, such as along Weld 2, can be conveniently generated by a simple transformation of the coordinate system.

4.8 APPLICATION RANGE OF THE MESH GENERATOR

The overall mesh generation procedure could be summarised in Figs. 4.26 - 4.29, in which the steps to generate each kind of mesh is described in details. The automatic mesh generator is able to cater for a wide range of geometrical parameters for partially

overlapped CHS K-joints with welding and crack details. However, there are still limitations on the mesh generator. The recommended range of geometry, in which the mesh generator can handle, is listed as below:

- Intersecting angle: $30^\circ \leq \theta_1, \theta_2, \theta \leq 90^\circ$
- Ratio of brace to chord radius: $0.1 \leq \beta_1, \beta_2 \leq 1.0$
- Ratio of brace's thickness to brace's radius: $0.03 \leq t/\gamma \leq 0.25$
- Percentage of overlap: $20\% \leq p \leq 80\%$
- Position of crack: anywhere along the welds at either the chord or brace sides.
- Crack surface angle: $-20^\circ \leq \omega \leq 20^\circ$
- Crack length: $4t_w \leq l_{cr} \leq l_{int} - 2t_w$
- Crack depth: $0.15 \leq a'/t \leq 0.85$

For the generation of the crack in the crack length direction, the entire crack model is built up by several individual blocks. Since the crack block mesh is a structured mesh, there should be at least four blocks to model the whole length of the crack as well as two other blocks to serve as transition zones. This observation can be transformed into the limit on the crack length. It is because the element size in the surface direction is determined to be approximately equal to the weld thickness. In addition, in the crack depth direction and for the crack angle, the targeted limit is set such that different kinds of element can be generated within the crack block without much distortion.

In particular, the mesh generator can handle special cases of identical chord and braces dimensions as well as large overlapped percentage of 80%, which are not commonly covered by other commercial software packages. Dexter and Lee (1999b) reported cases that numerical analysis cannot be performed due to limited capacity of the ANSYS. It should also be mentioned that, the mesh generator has been developed in such a way that the practice range specified by the CIDECT Guide (Zhao et al. 2000) can be covered. Note that other than the application for partially overlapped K-joint, the current mesh generation approach could also be applied to other commonly used joint types such as T, Y and gap K joints. As reported in the literature, the mesh generator developed for them (Lie et al. 2003) are limited to a maximum value of $\beta =$

0.8. An example of the mesh having β ratio equal to unity and overlap ratio equal to 80% is given in Fig. 4.30. Other than the case the two braces are similar in dimension, the case where the two braces have different dimensions, such as in a N-joint, can also be catered for as shown in Fig. 4.31. In fact, the mesh generator developed can be used directly for the fatigue study on partially overlapped N-joints.

4.9 CONCLUDING REMARKS

In this Chapter, an automatic mesh generation procedure for fatigue analysis of partially overlapped CHS K-joints is proposed. The mesh generator is able to generate several kinds of mesh for different analysis purposes. For example, mesh SURF_0W without any welding details could be extracted out for quick assessment of the fatigue strength of the joint while mesh HYBR_1W with welding details could be formed with little addition effort for quick SCF estimation. For a more intensive and detailed study on the distribution of HSS around the joint intersection, full solid meshes SOLID_1W and SOLID_0W with or without welding details could be used. Eventually, in the case that a full numerical study for a crack joint is needed, a full solid mesh with welding and crack details SOLID_CR can be generated. In particular, the mesh generator can handle special cases of identical chord and braces dimensions as well as large overlapped percentage of 80%, which are not commonly covered by other commercial software packages. Most importantly, it is able to generate a solid mesh with welding details and surface crack of any length and locates at either sides of the joint intersection.

Chapter 4 Mesh Generation for Partially Overlapped CHS K-Joints

Element types	No. of nodes	Shapes
1. Hexahedral/Cubic Element – (H8)	8	
2. Prism/Wedge – (P6)	6	
3. Tetrahedron – (T4)	4	
4. Pyramid – (PR8) (Collapsed Hexahedral)	8	

Table 4. 1 3D solid linear elements

Element Types	No. of Nodes	Shapes
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 4. 2 3D solid quadratic elements

Fig. 4. 1. Mesh generation scheme

1, 2, 3: number of sequence along which the surface mesh is created

Fig. 4. 2. Extrusion scheme

Fig. 4. 3. Surface mesh for individual zones in a partially overlapped CHS K-joint

Fig. 4. 4. SURF_0W with quadrilaterals in weld positions

Fig. 4. 5. Surface mesh with welding details, mesh HYBR_1W

Fig. 4. 6. Surface mesh with welding details, mesh HYBR_1W (interior view)

Fig. 4. 7. Solid mesh without welding details, mesh SOLID_0W (exterior view)

Fig. 4. 8. Solid mesh without welding details, mesh SOLID_0W (interior view)

Fig. 4. 9. Solid mesh with welding details, mesh SOLID_1W (exterior view)

Fig. 4. 10. Solid mesh with welding details, mesh SOLID_1W (interior view)

Fig. 4. 11. The welds

Fig. 4. 12. Common space between the welds

Fig. 4. 13. Positioning crack tips

Fig. 4. 14. Location of the surface crack on a local uv coordinate system

Fig. 4. 15. Parameters defining the crack surface

Fig. 4. 16. Flow chart of the extraction procedure

Fig. 4. 17. Mesh SOLID_1W with the extracted zone

Fig. 4. 18. Coordinate system to determine the crack front

Fig. 4. 19. Mesh design around the crack front

Fig. 4. 20. Crack ring

Fig. 4. 21. Crack block CRBLOCK

Fig. 4. 22. Transition blocks

Fig. 4. 23. Mesh SOLID_CR with surface crack on the brace side of Weld 1

Fig. 4. 24. Mesh SOLID_CR with surface crack on the chord side of Weld 1

Fig. 4. 25. Mesh SOLID_CR with surface crack on the chord side of Weld 3

Fig. 4. 26. Surface mesh generation

Fig. 4. 27. Solid mesh generation

Fig. 4. 28. Crack zone generation

Fig. 4. 29. Mesh combination and ABAQUS input file generation

Fig. 4. 30. Mesh of a partially overlapped CHS K-joint having $r/R = 1.0$, $p = 80\%$

Fig. 4. 31. Mesh of a partially overlapped CHS N-joint

CHAPTER 5

EXPERIMENTAL FATIGUE STUDY ON PARTIALLY OVERLAPPED CHS K-JOINTS

5.1 INTRODUCTION

Until now, the fatigue behaviour of CHS joints has been experimentally investigated for several joint types. For simple joints, static tests (Wordsworth 1981, Smedley and Fisher 1991, Karamanos et al. 1997, Chiew et al. 2004) and cyclic tests (Chiew et al. 2004 and Chiew et al. 2007) have been performed intensively. For more complex joints, static experimental results have been reported by Fung et al. (2001) for completely overlapped K-joints and Pang (2007) for partially overlapped KK-joints. However, for partially overlapped CHS K-joints, test result reported so far is only on the ultimate strength under AX and IPB loading (Dexter et al. 1996) which had been performed more than a decade ago. The only source of SCF equations for partially overlapped CHS K-joints by Efthymiou and Durkin (1985) under AX, IPB and OPB loadings are based merely on numerical parametric analysis and extrapolation from simple CHS T/Y-joints. In order to gain a better understanding on the fatigue behaviour of partially overlapped CHS K-joints, full scale fatigue tests should be carried out. The static test is able to produce SCF values which are then used to predict the total life of a joint under service load. However, the static test results cannot be used to assess the residual life of a joint which has crack-like fault. For this reason, the cyclic test can be utilised. However, for this approach the detailed crack information such as crack path, crack surface and crack propagation rate should be carefully studied.

In this current test program, two full-scale partially overlapped CHS K-joints were tested under cyclic combined loads in order to gather more information about the fatigue performance of the joints, as well as to evaluate the surface crack model proposed in the previous chapter. During the tests, crack initiation and propagation were monitored by the alternative current potential drop (ACPD) technique. The weld profile, shape and surface of the crack of the two full-scale specimens were measured.

The fatigue life corresponding to certain HSS range of each specimen is used to validate against the CIDECT S-N curve (Zhao et al. 2000).

5.2 TEST RIG AND SPECIMEN DETAILS

5.2.1 Test rig and cyclic load applied

The “orange” rig as shown in Fig. 5.1 was used to test the two partially overlapped CHS K-joints under basic combined loading conditions. The rig is capable of applying static loading on a joint specimen to determine the HSS distribution in the joint, as well as cyclic loading to study the fatigue performance of the tested joints. Load provided to the specimen can be along three mutually perpendicular axes in the form of AX, IPB and OPB by three actuators having capacity of 250, 250 and 150kN respectively as shown with the positive direction of loading in Fig. 5.2.

In the fatigue tests, sinusoidal constant amplitude loads controlled by the Instron RS Console software were applied to the specimens. The loads applied were actually combinations of basic AX and IPB loads with a frequency of 0.2Hz as shown in Fig. 5.3. However, for first specimen, a secondary OPB with a magnitude of 2kN acting downward as shown in Fig. 5.4 was applied to the through brace. This OPB loading was generated by the self weight and the misalignment of the specimen. The cyclic loads were selected in such a way that the resulted HSS at the weld are in the range of $[0.67 - 0.93] \sigma_y$ so that the specimens would fail in a realistic number of load cycles. For all load cases, the minimum magnitude of the applied load was zero while the maximum loads generated a tensile stress at a certain position along the joint intersection. During the test, all actuators were preset under the load control condition.

5.2.2 Design of two full scale specimens

The two partially overlapped CHS K-joint specimens, namely Specimens S1 and S2, were designed in such a way that if they are real structural components, overlapping is essential. The dimensions and non-dimensional parameters of the joints are given in Fig. 5.5. In fact, when the minimum requirement for gap joint is satisfied ($g \geq t_1 + t_2$), the eccentricity of Specimens S1 and S2 is $0.51R_0$ whereas the upper limit for eccentricity according to EC3 is $0.5R_0$. Therefore, it is necessary that the joints should

be overlapped to reduce the eccentricity. In this study, with 28.3% percentage of overlap, the eccentricity vanishes.

Specimens S1 and S2 were designed such as they have similar dimensions, except for the thickness of the chord and braces. It is because Specimens S1 and S2 were used to study the difference in HSS and fatigue crack locations on a particular specimen once the loading reverses. In addition, the chord length is designed more than 6 times its diameter to ensure that HSS in the joint intersection is not affected by the end conditions. For fabrication, hot finished structural steel pipe according to API 5L Grade B were used. The mechanical properties of the sections are given in Fig. 5.5.

5.2.3 Weld thickness measurement and validation

The weld profile and specimen preparation were carried out in accordance with the AWS (2008) specification. The weld quality was checked using ultrasonic technique. As the weld size has a significant effect on the stress distribution around the joint intersection, the actual weld size had been measured by a method presented in Chapter 3 and plotted against the modelled weld size and actual intersection as shown in Figs. 5.6 and 5.7 for the brace side of Weld 1 in Specimens S1 and the chord side of Weld 3 in Specimen S2 respectively. The complete weld thickness measurement for all weld paths of the two specimens are presented in Appendix C. It was assumed that the measured inner weld paths coincide with the modelled ones both at the chord and brace sides of the weld. It is because the joints could not be opened for measurement with the current equipments. In addition, since the requirement for the weld thickness on the brace side has not been specified in the AWS (1996) and API (2000), the same requirements for weld thickness on the chord side are applied to plot Fig. 5.6.

It can be observed that the modelled outer weld paths resembled the actual weld shapes. In addition, it can be seen that the measured outer weld paths are generally larger than the modelled profiles, especially at crown heels position. At the saddle position for Specimen S1, the geometrical outer weld model is closer to the measured outer weld path as shown in Fig. 5.6. However, the outer welding profile at this position is not as smooth as in other positions, which might be due to the effect of post weld grinding. For Weld 3 in Specimen S2, since the weld path is short and the weld

position is conveniently accessible, the actual weld profiles on the chord side can be considered smooth as the weld profile shows in Fig. 5.7a and the weld thickness illustrates in Fig. 5.7b. In addition, from the respective k_{Tw} -graphs on Figs. 5.6b and 5.7b, it can be concluded that measured weld thickness satisfies both the AWS (1996) and the API (2000) requirements. Measured weld thickness is generally larger than modelled weld, which shows that the geometrical model would be conservative when it is used to predict the SCF or SIF values. Therefore, the outer weld models for the chord and brace sides as described in Eqns. (3.47), (3.48) and (3.51) with parameters defined in Eqn. (3.72) can be used in the numerical modelling of partially overlapped K-joints.

5.3 HOT SPOT STRESS DISTRIBUTION

The distribution of HSS on the load combinations is shown in Figs. 5.8 and 5.9 for Specimen S1 and S2 respectively as reproduced from Lee et al. (2007a). The actual nominal tensile stresses caused by peak cyclic loading applied to the specimens measured by four midway brace member strain gauge readings are also recorded in each of the figures. For Specimen S1, the static test with a combination AX, IPB and additional OPB loads in positive direction having the nominal magnitude of 200kN, 45kN and 2kN respectively had caused HSS to occur along the weld intersection between the through brace and the chord. However, as shown in Fig. 5.8, the maximum HSS did not appear on the chord side but on the brace side of Weld 1. For Specimen S2, a similar load combination without the OPB load and with the IPB load in reverse direction generated HSS along the weld intersection between the overlap and through brace as shown in Fig. 5.9. If the through brace is considered as a secondary chord, then in this case, maximum HSS appeared at the chord's side following the ordinary norm for tubular joints.

5.4 MONITORING FATIGUE CRACK GROWTH

5.4.1 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. The ACPD technique uses an alternating current with a high frequency of about 5 kHz induced on the surface of

ferromagnetic material. Assuming there is a surface defect or crack as shown in Fig. 5.10, the alternating current flows down one surface crack and up the other crack side, a linear potential gradient is assumed to exist between them. Measurements of potential drops across the crack and adjacent to the crack allow the calculation of one dimensional crack depth as in Eqn. (5.1):

$$a = \frac{\Delta_R}{2} \left(\frac{V_C}{V_R} - \frac{\Delta_C}{\Delta_R} \right) \quad (5.1)$$

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 a is the crack depth at that particular probe or site.

Eqn. (5.1) is only applicable for an infinite long crack in an infinite plate. However, for fatigue cracks in tubular joints, it has been proved that the difference in crack depth measurement between the theoretically infinite long crack and a semi elliptical crack is negligible (Kam 1985). By the subtraction of the initial ACPD crack depth readings on the uncracked joint from all subsequent ACPD scans, an accurate measure of crack development can be obtained.

In general, direct determination of the crack lengths from ACPD crack depth data is difficult because the probes are fixed and the probes distance may be 10 mm or more apart in the region where the crack tips are expected. In this study, a simple method of using a second order polynomial curve to fit the depths of the three probe sites nearest the crack ends is applied. The extrapolated intersection of the fitting curve to the outer surface of the chord's wall determines the length of the crack. The comparison between the crack lengths produced using this method with that by visual measurements shows that it is suitable to estimate the crack lengths.

5.4.2 ACPD probe locations

The development of fatigue crack in the two partially overlapped CHS K-joints was captured by the ACPD technique through measurement points at ACPD probes. As a crack is expected to initiate at the peak HSS site and propagate in the vicinity of this region, fixed ACPD probes were fixed at region close to where the HSS occurred. For Specimen S1, static test had revealed that the maximum HSS did not appear on the

chord side but on the brace side of Weld 1. Therefore, ACPD probes were installed along the crown of Weld 1 on the through brace's surface as shown in Fig. 5.11. For Specimen S2, according to the static test result, HSS occurred on the chord side of Weld 3. Therefore, ACPD probes were fixed along the crown of Weld 3 on the through brace's surface as shown in Fig. 5.12.

The probes are specially manufactured with diameter of 0.18mm and length of 10mm in order to be spot welded easily and rigidly on the surface of the CHS. Along the weld the probes were distributed in three rows as shown in Fig. 5.13. The three probes having the same position along the weld formed a channel, eight of which made a field. On each specimen, four fields were used and the probe interval of 10mm was adopted. All probes were then connected by channels to the U10 Crack Microgauge (TSC 1991). This equipment with the capacity of utilising the ACPD technique can detect the size of defects in electrically conducting material. The U10 Crack Microgauge was then connected to a computer loaded with the Flair software (TSC 1998). The Flair software was written specifically for the use of the U10 Crack Microgauge so as to provide automatic instrument control, data storage facilities and delicate graphical output in a Windows environment.

5.5 FATIGUE TEST RESULTS

When the cyclic load is applied on a partially overlapped CHS specimen, a crack will be formed. If the crack is opened along its length, the crack front will appear as an elliptic-like shape which extends sideward when the number of cycles N increases. If a cross section is cut across the surface of the CHS such as it is perpendicular to the weld path as shown in Fig. 4.18, the deepest point could be seen progressively making a curve from the perpendicular line. The curved distance between the deepest point and the reference point is measured as the curved crack depth a' and the corresponding angle it makes to the perpendicular line is marked as ω as previously defined in Fig. 3.30. If the crack deviates outward, ω is given a negative value, otherwise it is positive. The crack angle is also observed to vary along the crack length, even though the variation is not very significant. In addition, since the crack profile is curve, $a' \geq a$ which is the projection of the crack curve on the perpendicular line. Therefore, when the crack penetrates through the full thickness of the tube, the ratio a'/t_0 is expected to be greater than unity.

A FORTRAN program was developed to analyze the raw ACPD crack depth data and to subtract the initial depths measured on the uncracked joint at each site from all subsequent measurements. The results obtained were used to plot several graphs relating crack parameters, as shown in Figs. 5.14 - 5.17 for Figs. 5.18 - 5.21 for Specimen S2. The ACPD record consists of crack data in both crack thickness and crack length directions. With the progressive growth of a crack, they can be defined as the penetration and propagation directions. The development history of the crack in both directions is plotted in both directions in Figs. 5.14 and 5.18 for Specimens S1 and S2 respectively. Each curve in the crack development history is smoothed out by a polynomial curve fitting. For the penetration direction, the relationship between the curved crack depth a' and the number of cycles N is plotted in Figs. 5.15 and 5.19 for the two specimens. The rate of penetration da'/dN is related to the ratio a'/t_0 in Figs. 5.16 and 5.20 for Specimens S1 and S2 respectively. For the propagation direction, graphs of crack length l versus crack depth a' as the crack extends are plotted in Figs. 5.17 and 5.21. In addition, as shown in Fig. 5.14, for some probe locations the crack information was not captured. It is because the crack branched out of the weld path as visually shown in Fig. 5.22.

In general, it is considered detrimental when the curved crack depth a' is equal to the thickness of the tube it penetrates at a number of cycles N_f . However, the fatigue life of the specimen is normally defined as N_3 which is the number of cycles when a through thickness crack is formed (Bowness and Lee 1995, Chiew et al. 2004). The S-N curve reproduced from the CIDECT Guide (Zhao et al. 2000) with thickness correction for Specimens S1 and S2 are compared with the fatigue life obtained from the test as shown in Fig. 5.23.

5.5.1 Fatigue test result for Specimen S1

For Specimen S1, static test with a combination AX and IPB load in positive direction, having nominal magnitudes of 200kN and 45kN respectively had caused the HSS to occur along the weld intersection between the through brace and the chord. The maximum HSS did not appear on the chord side but on the brace side of the weld. As shown in Fig. 5.13, a surface crack initiated near probe S1P15. However for almost all of its life, the deepest section of the crack shifted near to the probe S1P16 which was then slows down along the penetration procedure and gave way to the probe S1P14 to

go through the thickness of the through brace consequently. It can also be seen that the crack shape is slightly unsymmetrical with respect to the probe S1P15 as expected, which might due to the self weight and misalignment of the specimen. A secondary OPB with a magnitude of 2kN acting downward as shown in Fig. 5.3 is considered applied to the through brace causing the crack to tear up on the upper half of the specimen.

The initiation phase took around 18k cycles, which makes a proportion of about 30% of the total cycles N_3 applied as shown in Figs. 5.15 and 5.16. After the initiation phase, the crack develops steadily with a' increasing proportionally with N . It is noted that the crack length for partially overlapped CHS K-joints is remarkably shorter than that of the cracks on some simple CHS joints (Chiew et al. 2004). For the propagation of the crack, the crack length extends as its depth increases with a final ratio of 6.47 as shown in Fig. 5.17. In addition, the number of cycles N_f for the curved crack depth at the deepest point of the crack to reach a value equal to the thickness of the CHS and the number of cycles N_3 for the formation of the through thickness are 58k and 60k respectively. It can be observed the difference between the two numbers is small. This is quite similar to what has been tested for API pipes and can be contributed to the hydrogen embrittlement of the material (Huang 2003). As a result, the S-N curve with thickness correction for $t = 19.1\text{mm}$ when compared with the tested HSS range and number of cycles N_f and N_3 shown in Fig. 5.23 is conservative in predicting the fatigue life of Specimen S1.

5.5.2 Fatigue test result for Specimen S2

For Specimen S2, a similar load combination to what had been applied to Specimen S1, except that the IPB load was in reverse direction and caused the HSS to occur along the chord side of Weld 3 between the overlap and through braces as shown in Fig. 5.7. Subsequently, a crack initiated in the middle of probes S2P14 and S2P15 as shown in Fig. 5.18. From this figure, it can be seen that the deepest point of the crack generally remained there until it penetrated through the thickness of the through brace and the crack shape is considered symmetrical with respect to probe S2P15.

However, unlike the first specimen, in Specimen 2, the initiation phase accounts only for 17% of N_3 with approximately 12k number of cycles as shown in Figs. 5.19 and

5.20. Subsequently, the penetration rate remained steadily increase. For the propagation of the crack, the crack length extended as its depth increased with a final ratio of 5.46 as shown in Fig. 5.21. Again, it is seen that the crack is relatively short compared to previously tested CHS joints (Chiew et al 2004, Lie et al 2006). The number of cycles N_f for the deepest point of the curved crack depth reached the full thickness and the number of cycles N_3 for the formation of the through thickness are 69k and 91k respectively. As a result, the S-N curve with thickness correction for $t = 20$ mm when compared with the tested HSS range and criteria of failure according to the number of cycles N_f is unconservative. However, when the criterion for failure with the number of cycles N_3 is used, it can be seen from Fig. 5.23 that the CIDECT Guide (2000) is conservative in predicting the fatigue life of Specimen S2.

5.6 CRACK GEOMETRY MEASUREMENT

After the fatigue test, the joints were forced open along each crack surface. Visual observation indicates that for a partially overlapped CHS joint, the crack surface is not always perpendicular to the wall of the CHS. In order to obtain more details of the crack shape, the cracked joints were cut out from the specimens and split along the crack surface. The opened crack surfaces for Specimens S1 and S2 are shown in Fig. 5.24. Generally for both specimens, it is seen that the geometry of the crack surface changes along brace-chord intersection and is unsymmetrical with respect to the deepest point. In addition, for a given section, the absolute value of the angle ω decreases as the crack penetrates into the wall of the CHS. In order to gather further details on the crack surface angle ω , a simple clay moulding procedure similar to what has been described in Chapter 3 was employed to measure the variations of ω at different sections. The measurement of ω along the crack surface corresponding to different a'/t_0 values for Specimens S1 and S2 are plotted in Figs. 5.25 and 5.26 respectively. From these figures, it can be observed that:

- In general, ω decreases as a'/t_0 increases at all sections for both specimens.
- For Specimen S1, ω obtains negative values at the deepest point with quite a small range of $[-15^\circ, -5^\circ]$. Away from the deepest point, the range is larger as $[-20^\circ, 0^\circ]$ degree. Near the crack tip, the crack surface angle even achieves positive values.

- For Specimen S2, ω obtains positive values at the deepest point with quite a small range $[0^\circ, 7^\circ]$. Away from the deepest point, the range is larger but still within the range of $[3^\circ, 12^\circ]$.

From Figs. 5.25 and 5.26, it can be seen that although the variation of the crack surface angle ω depends on the welding details and loading applied, its variation is quite small and close to a perpendicular line to the wall of the CHS. Nevertheless, it is found that the information obtained could be used for the verification of SIF values using the proposed crack model in Chapter 6. In addition, the comparison between modelled and measured values of the final crack shape in terms of crack length and crack depth for Specimens S1 and S2 is plotted in Figs. 5.27 and 5.28 respectively. Although for some position along the crack length, the modelled crack shape curves display discrepancy to the measured ones, the assumption that the shape of the crack is bielliptical might still be used as an approximation to the crack front. It is because the numerical validation of SIF values with this assumption has produced reasonably good results.

5.7 CONCLUDING REMARKS

In this Chapter, two full-scale partially overlapped CHS K-joints were tested under cyclic combined loads. The weld profile and crack surface of the two full-scale specimens were studied using the clay model. Useful information of the crack location, penetration and propagation through the thickness of the wall of the CHS and along the weld has been obtained. It is interesting to note that the fatigue test with a combination AX and IPB load in positive direction, having nominal magnitudes of 200kN and 45kN respectively had caused the crack to occur along the weld intersection between the through brace and the chord. It is seen that the fatigue life of the two specimens were relatively short as the crack penetrated into the thickness of the wall of the CHS rather than propagated sideward. The crack length was therefore resulted shorter than the crack length measured in other tests of CHS joints. It is also noted that under a certain load combination, the crack is able to occur at the brace side instead of the chord side of the weld. When compared with the S-N curve for tubular joints in the CIDECT Guide (Zhao et al. 2000), the fatigue life of Specimen S2 shows unconservative value if the number of cycles N_f when the crack first reach the full thickness of the CHS is used. Other than that, the S-N curves are conservative.

Fig. 5. 1. Test rig and specimen installation

Fig. 5. 2 Actuators to apply loads

Fig. 5. 3. Cyclic loads applied to Specimens 1 and 2

Fig. 5. 4. Additional OPB applied to Specimen S1

Specimens	Dimensional parameters (mm, degree)						E (GPa)
	D	T	d	t	θ_1	θ_2	
S1	273.0	25.0	244.5	19.1	45	45	204.42
S2	273.0	25.0	244.5	20.0	45	45	201.90
Non-dimensional parameters							
	ξ	γ	$\gamma_1 = \gamma_2$	β	β'	$\tau_1 = \tau_2$	
S1	0.35	7.15	6.40	0.89	1.00	0.764	
S2	0.35	6.83	6.11	0.89	1.00	0.800	

Fig. 5. 5. Specimen details

(a) Actual weld size

(b) k_{Tw} graph

Fig. 5. 6. Weld size along the brace side of Weld 1 for Specimen S1

(a) Actual weld size

(b) k_{Tw} graph

Fig. 5. 7. Weld size along the chord side of Weld 3 for Specimen S2

Fig. 5. 8. HSS distribution around Weld 1 for Specimen 1 under combined loading

Fig. 5. 9. HSS distribution around Weld 3 for Specimen 2 under combined loading

Fig. 5. 10. ACPD theory and notation

Fig. 5. 11. Probe location in Specimen 1

Fig. 5. 12. Probe location in Specimen 2

Fig. 5. 13. A close-up view of ACPD probe sitting

* Note: S2P0 is the probe having position of 0mm along the x-axis

Fig. 5. 14. Curved crack depth (a') history for Specimen 1

Fig. 5. 15. Crack penetration curve at Section S1P15

Fig. 5. 16. Crack penetration rate (da'/dN) at Section S1P15

Fig. 5. 17. Crack length l vs crack depth a' at Section S1P15

* Note: S2P0 is the probe having position of 0mm along the x-axis

Fig. 5. 18. Curved crack depth (a') history for Specimen 2

Fig. 5. 19. Crack penetration curve at Section S2P15

Fig. 5. 20. Crack penetration rate at Section S2P15

Fig. 5. 21. Crack length l vs crack depth a' at Section S2P15

Fig. 5. 22. Crack branching in Specimen 1

(a) for Specimen S1

(b) for Specimen S2

Fig. 5. 23. Fatigue test results comparing with SN curve data

(a) Opened crack shape for Specimen S1

(b) Opened crack shape for Specimen S2

Fig. 5. 24. Final crack shape in Specimens S1 and S2

Fig. 5. 25. Measured values of ω for Specimen S1

Fig. 5. 26. Measured values of ω for Specimen S2

Fig. 5. 27. Measured and modelled crack shape for Specimen S1

Fig. 5. 28. Measured and modelled crack shape for Specimen S1

CHAPTER 6

NUMERICAL ANALYSIS OF PARTIALLY OVERLAPPED CHS K-JOINTS

6.1 INTRODUCTION

Fatigue damage is the most frequent single cause of repair for offshore steel structures. However the assessment of the fatigue strength and service life of tubular joints in offshore structures is not a trivial task due to the complex geometry of the weld and crack details. For example, many welding details, such as weld thickness on the chord and brace sides, are highly variable and depend on the welding process and the plate thickness. In addition, cracks could be located at either the chord or the brace sides of the weld. Furthermore, in the case of a complex joint, such as a partially overlapped CHS K-joint, as the number of welding curves increases, the number of geometrical parameters multiples. However, in the practical application of fatigue study for tubular joints, it is assumed that only some parameters are critical in the assessment of the fatigue strength or residual life of the joints. In the literature, many studies have been carried out to determine the fatigue performance of welded joints (Dijkstra and Noordhoek 1985, Puthli et al. 1988, van Wingerde 1995) and the results have been included in the CIDECT Guide (Zhao et al. 2000) and other design guidelines.

In Chapter 5, it can be seen that full scale fatigue tests are able to provide valuable information on the fatigue performance of partially overlapped CHS K-joints. However, if a thorough understanding of the fatigue behaviour of the joint is required, due to time, cost and instrument limit it is impossible to carry out many tests under different load cases and boundary conditions. Towards this end, the numerical analysis is an alternative and less expensive way to study the fatigue behaviour of the tubular joints. However, the accuracy of the numerical results depends very much on the geometrical model employed and the quality of the mesh used in the FE analysis, especially for problems involving high stress concentration in small regions such as the

determination of SCF and SIF values. In addition, although the mesh generator introduced in Chapter 4 is able to model actual weld and crack profiles, it would be unrealistic to include such measurement in the design and appraisal of practical partially overlapped CHS K-joints. The geometrical models for welding and crack details described in Chapter 3 therefore have to be validated to the numerical model. Hence, in this Chapter, the numerical SCF and SIF values obtained from the FE models with exact geometry are compared with the respective results obtained from the two tested partially overlapped CHS K-joints. In addition, several FE models for the assessment of SCF and SIF values are presented for comparison and consideration. Furthermore, the numerical models with surface cracks are used to estimate the remaining life for partially overlapped CHS K-joints. Finally a numerical model is proposed for the practical assessment of SCF, SIF and the residual life.

6.2 NUMERICAL MODELLING OF THE TESTED SPECIMENS

For the numerical analysis of partially overlapped CHS K-joints, the modelling technique proposed in Chapter 4 is used. To validate the numerical model in terms of SCF and SIF approaches, two partially overlapped CHS K-joint specimens described in Chapter 5 are modelled with the measured weld thickness and crack surface geometry. For the SCF evaluation, besides the numerical model with exact geometry, the weld path model incorporated in the mesh SOLID_1W and the model without any welding details as generated in the mesh SOLID_0W are analysed. Subsequently, the numerical results are compared with the experimental results. For the SIF evaluation, several crack surface angle is considered in the generation of the mesh SOLID_CR. For all numerical models tested, the boundary, loading conditions and material properties are identical as the respectively tested specimens as shown in Fig. 6.1. The combined loading conditions as $200AX + 45IPB + 2OPB$ (kN) for Specimen S1 and $200AX - 45IPB$ (kN) for Specimen S2 are applied at the end of the through brace. In general, the material properties in the numerical models are specified such that they are identical to the tested specimens. However, the Young's modulus E at the ends of the chord, the through brace and overlap brace is increased in order to avoid localised failure at these positions.

6.3 FE ANALYSIS OF SCF VALUES ON PARTIALLY OVERLAPPED CHS K-JOINTS

6.3.1 Calculation of nominal stress

The SCF is defined as the ratio between the HSS and the nominal stress. The HSS can be estimated by field measurement, parametric equation or direct numerical analysis. The nominal stress can be calculated by simple beam theory and the superposition method. For basic loading cases, i.e. AX, IPB and OPB, the nominal stresses can be obtained as follows:

For AX load,

$$\sigma_n = \frac{4F}{\pi(D^2 - d^2)} \quad (6.1)$$

For IPB or OPB load,

$$\sigma_n = \frac{32DM}{\pi(D^4 - d^4)} \quad (6.2)$$

where F is the axial load, D and d are the outer and inner diameters of the brace, M is either the IPB or OPB moment. In this case, the position of σ_n for IPB and OPB loads is different by a quarter circle rotation. If Eqns. (6.1) and (6.2) are used to superimpose the nominal stresses of a tubular joint subjected to combined loading, the stress distribution on the outer surface of the brace can be calculated as follows:

$$\sigma_n = \frac{4F}{\pi(D^2 - d^2)} + \frac{32DM_{IPB}}{\pi(D^4 - d^4)} \sin \alpha + \frac{32DM_{OPB}}{\pi(D^4 - d^4)} \cos \alpha \quad (6.3)$$

with α is the driving angle, defined to be zero at the saddle of the joint intersection. The value of σ_n varies along the joint intersection as angle α changes. However, it is assumed that the maximum stress obtained from Eqn. (6.3) is the nominal stress of the tubular joint under combined load:

$$\sigma_n = \frac{4F}{\pi(D^2 - d^2)} + \frac{32D}{\pi(D^4 - d^4)} \sqrt{M_{IPB}^2 + M_{OPB}^2} \quad (6.4)$$

The position of the maximum nominal stress can be inferred from the ratio between the IPB and OPB moments:

$$\alpha = \frac{\pi}{2} - \tan^{-1} \frac{M_{OPB}}{M_{IPB}} \quad (6.5)$$

It is noted that Eqn. (6.4) can be considered a general expression of the nominal stress for CHS joints. For example, if a joint is subjected only to AX load, the terms M_{IPB} and M_{OPB} cancel out; Eqn. (6.4) reduces to the same Eqn. (6.1). In addition, it is also noted that the position of the nominal stress provided by Eqn. (6.5) is generally not the point where HSS occurs.

6.3.2 Parametric equations for SCF values for partially overlapped CHS K-joint under unbalanced loading

To date, the analysis of SCF for partially overlapped CHS K-joint has yet been specified in any code of practice, neither as it is thoroughly investigated experimentally and numerically elsewhere. The most recognised research work on SCF of partially overlapped CHS K-joints is presented by Efthymiou and Durkin (1985), in which the stress of over 100 tubular partially overlapped CHS K-joints was analysed using the PMBSHELL software. This set of parametric equations was experimentally verified by Dharmavasan and Seneviratne (1986) through four strain-gauged acrylic models. In the established set of parametric equations, several load cases were considered. However, large overlap was not considered in this analysis due to the limitations of the PMBSHELL mesh generator. It is noted that the set of equation results in the maximum SCF value but does not specify the exact location of the SCF, as well as the SCF distribution along the joint intersection. In this study, Efthymiou's set of parametric equation is compared with the SCF obtained from the numerical and experimental investigations. The comparison results are presented in the next Section. The parametric equations for unbalanced load cases are summarised below:

- For AX load:

- Chord:

$$SCF_{C,AX} = [T_1]_A + \left[\frac{\sin \theta_{\max}}{\sin \theta_{\min}} \right]^{0.8} [T_1]_B - [K_1] \quad (6.6)$$

- Brace:

$$SCF_{B,AX} = [T_3]_A + \left[\frac{\sin \theta_{\max}}{\sin \theta_{\min}} \right]^{0.8} [T_3]_B - 0.3[K_2] \quad (6.7)$$

where $[T_1]$ and $[T_3]$ are the SCF equations for the chord and brace saddle of a T-joint under AX load, $[K_1]$ and $[K_2]$ are the SCF equations for the chord and brace of a gap K-joint under AX load respectively:

$$T_1 = \gamma \tau^{1.1} [1.11 - 3(\beta - 0.52)^2] \sin^{1.6} \theta \quad (6.8)$$

$$T_3 = 1.3 + \gamma \tau^{0.52} \alpha^{0.1} [0.187 - 1.25 \beta^{1.1} (\beta - 0.96)] \sin^{(2.7-0.01\alpha)} \theta \quad (6.9)$$

$$K_1 = \tau^{0.9} \gamma^{0.5} (0.67 - \beta^2 + 1.16\beta) \sin \theta_{\max} \left[\frac{\sin \theta_{\max}}{\sin \theta_{\min}} \right]^{0.3} [1.64 + 0.29 \beta^{-0.38} \tan^{-1}(8\zeta)] \quad (6.10)$$

$$K_2 = 1 + K_1 (1.97 - 1.57 \beta^{0.25}) \tau^{-0.14} \sin^{0.7} \theta_{\max} + \beta^{1.5} \gamma^{0.5} \tau^{-1.22} \sin^{1.8} (\theta_{\max} + \theta_{\min}) [0.131 - 0.084 \arctan(14\zeta + 4.2\beta)] \quad (6.11)$$

- For IPB load:

- Chord:

$$SCF_{C,IPB} = 1.74 \beta \tau^{0.85} \gamma^{(1-0.68\beta)} \sin^{0.7} \theta \quad (6.12)$$

- Brace:

$$SCF_{B,IPB} = (1 + 0.65 \beta \tau^{0.4} \gamma^{(1.09-0.77\beta)} \sin^{(0.66\gamma-1.16)} \theta) (0.9 + 0.4\beta) \quad (6.13)$$

- For OPB load:

- Chord:

$$SCF_{C,OPB} = [T_{10}]_A [1 - 0.08(\beta \gamma)^{0.5} \exp(-0.8x)] + [T_{10}]_B [1 - 0.08(\beta \gamma)^{0.5} \exp(-0.8x)] [2.05 \beta^{0.5} \exp(-1.3x)] \quad (6.14)$$

where $[T_{10}]$ is the SCF equation for the chord saddle of a T-joint under OPB load:

$$T_{10} = \gamma \tau \beta (1.7 - 1.05 \beta^3) \sin^{1.6} \theta \quad (6.15)$$

and:

$$x = 1 + \frac{\zeta \sin \theta_A}{\beta}$$

- Brace:

$$SCF_{B,OPB} = SCF_{C,OPB} [\tau^{-0.54} \gamma^{-0.05} (0.99 - 0.47\beta + 0.08\beta^4)] \quad (6.16)$$

From Eqns. (6.6) - (6.16), it can be seen that the only the SCF at the weld connecting

the chord and the through brace is considered. Other possible SCF locations, such as positions along Weld 2, Weld 3 are not yet mentioned.

6.3.3 Comparison of SCF values between experiment data, Efthymiou's value and numerical results

The two tested partially overlapped CHS K-joints are modelled using the technique presented in Chapter 4 with the measured weld thickness, the modelled weld thickness (mesh SOLID_1W) and without welding details (mesh SOLID_0W). The analysis was conducted by using the general purpose FE program ABAQUS (2006) and the HSS was obtained by using the quadratic extrapolation method. The extrapolation procedure is similar to what has been done to obtain the SCF values at the weld path in the experimental analysis. Except for the mesh SOLID_0W, the extrapolation procedure is carried out for the intersection curves. It is because for a mesh without any welding details, the intersection curves is where the geometric discontinuity happens and acts as a stress riser. The numerical SCF distribution around Weld 1 for Specimen S1 subjected to AX, IPB, OPB and combined loadings, is shown in Figs. 6.2 - 6.5 respectively. An illustration of the deformation shape and the stress distribution around Weld 1 is given in Fig. 6.6. Similarly, for Specimen S2, the SCF values for all numerical models subjected to AX, IPB and combined loadings are compared with the experimental results in Figs. 6.7 - 6.9 and the deformation shape is shown in Fig. 6.10. The parametric SCF values calculated using Efthymiou's equations are also presented in the figures for comparison.

For Specimen S1, in general higher SCF distributions are predicted along both the chord and brace sides of the weld in all numerical models. In addition, it can be observed from Figs. 6.2 - 6.5 that the SCF obtained from the numerical model with measured weld thickness is able to give the best prediction among all the models with a maximum relative error of 11.57% near the heel area. However, when it comes close to the intersection point, the prediction of the SCF is not conservative. It might be due to the complex notch effect of the actual weld that has not been catered for in the geometrical and the numerical models. Nevertheless, as the SCF at that location is the lowest along the intersection, such underestimation does not impair the practical value of the models. Furthermore, the numerical models based on the meshes SOLID_1W and SOLID_0W reproduce a similar trend for the SCF estimation on the heel and

saddle sides of the weld. If the SCF values from the two meshes SOLID_1W and SOLID_0W are to be compared, it can be observed that the latter is able to give more conservative results than the former. The relative difference in the SCF prediction between the two meshes can be up to 22.86% for the saddle area on the brace side of Weld 1 in the case of AX loading as shown in Fig. 6.2. In addition, the mesh SOLID_1W is able to provide less overestimated SCF values meanwhile still be conservative with a maximum relative error of 15.83%.

If the parametric equation by Efthymiou (1985) is used to predict the SCF value for Specimen S1, it can be seen that the parametric results are not always conservative and the relative error compared to the test results can be up to 36.11% underestimated for the case AX loading as shown in Fig. 6.2b. In addition, the maximum percentage of overestimated for some load case could be as high as 169.51% for the case of IPB loading as shown in Fig. 6.3b. As a result, if the parametric equation is used for the estimation of SCF values for the combined loading as shown in Fig. 6.5, the SCF values on the chord and brace sides of the weld might only have a small difference (2.13 compared with 2.29 respectively). However, the inconsistency in SCF prediction for different basic load cases might cause uncertainty for the SCF prediction in combined load cases where the proportion of each basic load cases changes. In addition, it is also noted that the available parametric equation only gives the maximum SCF value but not the position. In case there is a large contribution of OPB loading and the HSS occurs at the saddle of the weld, it would be difficult to determine the exact location of the HSS.

Similarly for Specimen S2 as shown in Figs. 6.7 - 6.9, the SCF predicted by all numerical models are generally conservative and the SCF from the measured weld thickness is able to give the best prediction with a maximum relative error of 8.52%. Among the solid FE models, mesh SOLID_0W is able to produce the most conservative SCF prediction. The largest relative error produced by the mesh SOLID_1W and the mesh SOLID_0W are 16.14% and 32.18% respectively in the case of IPB loading. It is also noted that the parametric equation by Efthymiou (1985) only provides prediction for the chord and brace. No distinction is provided between the through and overlap braces, therefore in Figs. 6.7 - 6.9 only the prediction for the through brace is calculated. Furthermore, it is observed that the prediction for AX

loading is overestimated by 150.65%. However as the contribution of HSS due to AX loading is small, the final parametric prediction of SCF for combined loading is reasonable with 22.59% maximum relative error.

From the SCF results obtained from the experimental and the numerical models as well as from the established parametric Efthymiou's equation, it can be seen that the numerical models are able to reasonably produce the SCF values for the tested partially overlapped CHS K-joints. Furthermore, in case that the actual weld thickness is not available, the mesh SOLID_1W is recommended for SCF prediction.

6.4 FE ANALYSIS OF SIF ON PARTIALLY OVERLAPPED CHS K-JOINTS

6.4.1 SIF from experimental investigation

In order to verify numerical SIF values obtained from the modeled partially overlapped CHS K-joint, the experimental results from the full scale fatigue tests conducted in Chapter are converted to SIF values by the Paris' law:

$$\frac{da'}{dN} = C(\Delta K)^m \quad (6.17)$$

where da'/dN is crack growth rate which can be obtained from experimental data, ΔK is the range of SIF, C and m are material constants corresponding to API-5L pipes tested in ambient temperature condition. The values of $C = 1.427 \times 10^{-12}$ (m/cycle)(MPa $\cdot$ m $^{1/2}$) $^{-3.523}$ and $m = 3.523$ (Barsom and Novak 1977) are used in this study.

6.4.2 SIF from numerical investigation

The numerical value of SIF for partially overlapped CHS K-joint can be either implicitly or explicitly calculated by the FE package ABAQUS 6.5 (2006). ABAQUS is able to provide SIF values in terms of J -integral and K -factor. The J -integral is usually used in rate-independent quasi-static fracture analysis to characterize the energy release associated with crack growth. It can be related to the stress intensity factor if the material response is linear as expressed in the following formula:

$$K_I = \sqrt{JE'} \quad (6.18)$$

where $E' = E$ for plane stress and $E' = E/(1 - \nu^2)$ for plane strain problems and E is the Young's modulus. However, Eqn. (6.18) cannot be applied in mixed mode situations thought to be presented in tubular joints (Bowness and Lee 1995). For this reason, ABAQUS (2006) provides an interaction integral J_{int} to calculate the SIFs for different modes through a pre-logarithmic factor matrix $\mathbf{B}$ (Shih and Asaro 1988) as:

$$\mathbf{K} = 4\pi\mathbf{B}\mathbf{J}_{\text{int}} \quad (6.19)$$

After the components K_I , K_{II} , K_{III} of $\mathbf{K}$ are obtained through Eqn. (6.19), an equivalent SIF called K_e is assumed as the crack driving force for a mixed mode fracture problem (Chong Rhee et al. 1991). The relationship between K_e and K_I , K_{II} , K_{III} is:

$$K_e = \left[K_I^2 + K_{II}^2 + \frac{K_{III}^2}{1 - \nu} \right]^{\frac{1}{2}} \quad (6.20)$$

6.4.3 Numerical SIF values estimation and validation

The two partially overlapped CHS K-joints with measured crack geometry described in Chapter 5 are numerically discretized by the mesh generation procedure described in Chapter 4 and analyzed by the FE program ABAQUS (2006). However, as the measurement of the crack profile is only provided in scattered points whereas the actual crack front is a smooth space curve, a five-order polynomial is used to interpolate the crack surface angle based on the information provided in Figs. 5.25 and 5.26 and the results are then fed into the mesh generation program. The FE results are presented in Figs. 6.11 - 6.13 for Specimen S1 and Figs. 6.14 - 6.16 for Specimen S2. It is noted that these numerical SIF results are the average values from different contours except the first contour abutting the crack tip because numerical tests have shown that this contour cannot provide highly accurate results (ABAQUS 2006).

For the extraction of SIF values from the numerical integration, the output parameter K -factor obtained from the interaction integral method described in Eqns. 6.19 and 6.20, or the general K_J from the J -integral described in Eqn. 6.18 is used. Lie et al. (2004) has evaluated the accuracy of the interaction integral method through a comparison of SIF values obtained from this method and the displacement extrapolation method. The equivalent K_e values obtained from the K -factor analysis

were found to be close to the results evaluated by the displacement extrapolation method with plain strain assumption except for the points of crack tip on chord surface.

For tubular joints, especially joints with complex configuration, it is supposed that the mixed mode situation presented (Bowness and Lee 1995). For the evaluation of the individual SIF values of each crack opening mode and the equivalent SIF K_e , a crack having the length $l_{cr} = 83.28$ mm and the depth $a' = 6.48$ mm is selected for Specimen S1. Under the same loading condition as described in Sections 5.2, the overall deformation and crack opening displacement is illustrated in Fig. 6.11. The values of K_I , K_{II} , K_{III} and K_e obtained from the K -factor extraction are shown in Fig. 6.12. As expected, the values of K_I and K_e are close, while K_{II} and K_{III} contribute only a small percentage in K_e . From the figures, it is also observed that the value of K_{II} is much smaller compared to K_I . In fracture mechanics, K_{II} is a factor characterised the crack propagation direction. When K_{II} is considered small compared to K_I , it is supposed that the variation in the crack propagation direction in comparison with the perpendicular line is negligible and the relative movement between the two crack surfaces is predominantly opening mode (Mode I). Therefore, the relatively small magnitude of K_{II} can be used to explain the little variation in the crack surface angle measured in Section 5.6.

Similarly, for Specimen 2 under the same load combination as described in Section 5.2, a crack with length $l_{cr} = 82.29$ mm and depth $a' = 13.83$ mm located at the chord side of Weld 3 is generated in the numerical model. Again, the crack opening displacement shown in Fig. 6.14 and the comparison of SIF values for different crack opening modes shown in Fig. 6.15 illustrate that the opening mode (Mode I) is dominant for when the joint was subjected to combined AX and IPB loads. It is also seen in Figs. 6.12 and 6.15 that the value of equivalent SIF K_e obtained from the interaction integral and K_I obtained from the J -integral are almost similar. In addition, the values of K_e at the deepest points are higher than that at the two crack tips. If the maximum crack driving force is positioned at the deepest point, it can be concluded that the crack is able to penetrate through the thickness with a higher rate compared to the rate of sideward propagation. This observation might be used to explain for the small ratio between the crack length and depth l_{cr}/a' described in Section 5.5.

It is also noted that the SIF values obtained from the test results along a particular crack front which has the deepest point equal to 6.48mm for Specimen S1 and 13.83mm for Specimen S2 are also plotted in Figs.6.12 and 6.15 respectively. From the graphs, it can be observed that the numerical values are in good correlation with the measured curves since the maximum difference between them for Specimens S1 and S2 are 12.53% and 14.67% respectively. It should be noted that this comparison has excluded the SIF values near the two crack tips, since the measurement at these location is not always reliable.

The numerical SIF result obtained from the mesh with measured crack surface angle is compared with the experimental result at the crack deepest point in Fig. 6.13 for Specimen S1 and Fig. 6.16 for Specimen S2. From the figures, it can be observed that both the trend and values of the numerical SIFs match well with the experimental results. The maximum relative difference of the SIF results from experimental and numerical analyses is less than 10%. However, it is noted that the FE model for Specimen S1 underestimates the SIF values in the initiation phase of the crack development, where the ratio a'/t_0 is less than 0.25. For Specimen S2, it is noted that although the relative error in the estimation is 5.68%, the SIF values estimated are on the unconservative side.

From above comparisons and analysis, the validity of the numerical model for partially overlapped CHS K-joints with crack is confirmed. It is shown that the mesh generation technique proposed in Chapter 4 can be considered stable and reliable for further numerical analysis of partially overlapped CHS K-joints with crack.

6.4.4 Comparison of SIF from different numerical models

In the literature, the geometrical and numerical modeling of CHS joints was studied by many researchers (Rhee et al. 1991, Cao et al. 1997, Lee et al. 1999, and Lie et al. 2003). However, most of the studies assumed that the crack surface is perpendicular to the chord wall. Although for CHS joints, it has been concluded that the SIF values derived from a straight perpendicular crack is not significantly different from that for a crack with the correct crack surface (Bowness and Lee 1995), the investigation on the variation of SIF with different crack surface angles is still of interest in this study. Note that with a general geometrical modelling of the crack surface and the mesh generation

procedure that allows for the crack surface to vary from -20° to 20° , the slightly inclined crack surface as described in Section 5.6 can be easily generated and used.

Generally, the two full scale partially overlapped CHS K-joint specimens tested in Chapter 5 are modelled with several constant crack surface angles. The choice for a constant crack surface is based on the observation in Section 5.6 that the crack surface angle has little variation along the crack front. It is also because the measurement of the crack surface angle is not always available, and in that case, an assumption on the crack surface angle should be made. Besides the variation in the crack surface angle, other parameters such as the length and the depth of the surface crack are identical to the measurement from the ACPD record. Subsequently, the SIF values at the deepest point obtained from different numerical models are compared with the experimental observations as shown in Figs 6.17 and 6.18. Based on the comparison, a practical crack surface angle is proposed.

For Specimen S1, the crack surface angle for a crack situated on the brace side of Weld 1 ranges from $\omega = 0^\circ$ to -15° . The reason for the use of negative crack surface angle ω is based on the observation that the actual crack surface propagates outward from the weld. In contrast, for Specimen S2 the crack surface angle for a crack situated on the chord side of Weld 3 ranges from $\omega = 0^\circ$ to 15° .

For Specimen S1, the SIF results at the deepest point from four different crack surface models are compared with the benchmark experimental result in Fig. 6.17. It can be seen that the models with smaller absolute value of crack surface angle ($\omega = 0^\circ, -5^\circ$) give SIF values closer to the experimental values at the initiation phase of the crack propagation ($a'/t_0 \leq 0.35$). On the other hand, the FE models with larger absolute value of crack surface angle ($\omega = -10^\circ, -15^\circ$) give SIF values closer to the experimental values at the end of the crack propagation ($a'/t_0 > 0.40$). This observation can be explained by the larger absolute values of the crack surface angle at the deepest point as the crack penetrates in the thickness direction. This might be an explanation for the observed differences in predicted SIF values from different crack surface models. However, overall it can also be concluded that good correlation with test results can be obtained for all models, among them the crack surface $\omega = 0^\circ$ is able to achieve a closest approximation to the experimental result with a maximum relative error of

13.24%.

For Specimen S2, generally all models result in underestimation of SIF values, especially when $a'/t_0 \leq 0.6$ as shown in Fig. 6.18. For example in the numerical model with the crack surface $\omega = 15^\circ$, the SIF is underestimated by 22.36% in the range $a'/t_0 \leq 0.6$, where as the maximum error in the range $a'/t_0 > 0.6$ is only 7.51%. This might attribute to the stronger variation in the crack surface angle as shown in Fig. 5.26 where the crack surface angle varies more than 5° within a 10 mm range. However, from Fig. 6.18, it can also be observed that acceptable correlation with test results can be obtained for the crack surface $\omega = 0^\circ$ with a maximum error of 15.83%.

As observed in Figs. 6.17 and 6.18, all numerical models predict unconservative values when the parameter a'/T is small. In details, the maximum relative error when $a'/T \leq 0.35$ for Specimen S1 is 12.37% and the same parameter when $a'/T \leq 0.6$ for Specimen S2 is 22.36%. This underestimation might be contributed to the effect of curvy crack surface. It is noted that the crack surface is curved in two directions, which is along the crack length and down to the crack depth. While the current model has accounted for the curvy shape in the crack length direction through the introduction of the crack surface angle, the crack shape through the crack depth direction has been assumed to be flat through the use of one element layer in the generation of the crack block. When the crack is shallow, i.e. the value of a'/T is small, the effect of the curvy crack shape in the crack depth direction might be important to the estimation of SIF values for partially overlapped CHS K-joints.

From the comparison of SIF values obtained from different crack surface models and for crack situated both on the chord and brace sides of the weld, it can be concluded that if the exact geometry of the crack surface angle is not available, a value of $\omega = 0^\circ$ is recommended for the numerical estimation of the SIF values for partially overlapped CHS K-joints.

6.5 FATIGUE LIFE PREDICTION FOR PARTIALLY OVERLAPPED CHS K-JOINTS

6.5.1 Scope of life prediction using fracture mechanics approach

The presence of a crack can significantly reduce the strength of a tubular joint.

However, it is unusual for a crack of dangerous size to exist in the first instance. A more common situation is that a small flaw that was initially present develops into a crack and then grows until it reaches the critical size. As discussed in Section 2.3.2, indicated in Fig. 2.7, and reproduced in Fig. 6.19, the fatigue crack growth rate comprises of three regions (a) slow growth near the threshold ΔK_{th} below which crack growth does not occur, (b) intermediate region following a power equation, and (c) unstable rapid growth prior to final failure of the specimen. The value of ΔK in the third region often asymptotically approaches the fracture toughness K_C of the material (Dowling 1999). In the second region, the relationship between ΔK and da'/dN can be represented as a straight line on a log-log plot, which is identified as the Paris' law. Although a number of empirical equations are available for the combined second and third stages (Weertman 1966, Foreman et al. 1967), or for the entire fatigue growth curve (McEvily 1988), the crack propagation stage is crucial and limited in this study. The reason for this determination lays in the long duration of the propagation stage and its negligible dependency on the microstructure and monotonic flow properties within a given material so that continuum fracture mechanics can be applied.

6.5.2 Life prediction procedure for partially overlapped CHS K-joints

In fracture mechanics, the concept of similitude applies. It is implied that the fatigue crack growth is uniquely defined by a single loading parameter such as the SIF (Anderson 1991). For example, the crack growth rate da'/dN for a given combination of material and stress ratio $R = \frac{\sigma_{min}}{\sigma_{max}} < 0$ can be expressed in the form of Paris' law

(Eqn. 6.17) where any effects of environment, frequency are assumed to be included in the material constants C and m involved. Inversely, if the value of SIF is considered known in some intervals of the crack life, the number of cycles required for crack growth can be calculated by the integration of Eqn. (6.17):

$$\int_{N_{initial}}^{N_{final}} dN = N_{final} - N_{initial} = \int_{a'_{initial}}^{a'_{final}} \frac{da'}{C(\Delta K)^m} \quad (6.21)$$

The integral (6.21) gives the number of cycles required for the crack to grow from an initial size $a'_{initial}$ at cycle number $N_{initial}$ to a final size a'_{final} at cycle number N_{final} . Supposed that ΔK can be approximated by numerical analysis as in Section 6.4, then

Eqn. (6.21) can be applied to estimate the residual life, $N_r(a') = N_{\text{final}} - N_{\text{initial}}$. However, as ΔK can only be approximated at discrete values of a' in the range $[a'_{\text{initial}}, a'_{\text{final}}]$, numerical integration is required to compute Eqn. (6.21). If the rate of accumulation of cycles N per unit of increase in crack depth a' , is defined as dN/da' (Dowling 1999), and

$$\frac{dN}{da'} = \frac{1}{da'/dN} = \frac{1}{C(\Delta K)^m} \quad (6.22)$$

then Eqn. (6.21) is equivalent to:

$$N_r(a') = \int_{a'_{\text{initial}}}^{a'_{\text{final}}} \left(\frac{dN}{da'} \right) da' \quad (6.23)$$

To numerically integrate Eqn. (6.23), a plot of dN/da' versus a' as shown in Fig. 6.20 can be used. The life $N_r(a')$ is simply the area under the curve between a'_{initial} and a'_{final} . As a result, the accuracy of $N_r(a')$ depends on the accuracy of ΔK estimated, and the value of a'_{final} set to be the failure criteria for the partially overlapped CHS K-joint.

6.5.3 Life prediction for tested specimens

By integrating Eqn. (6.23), the SIFs obtained from different crack surface angle models are employed to predict the residual life for the tested specimens. The detailed inputs for the residual life calculation are listed below.

For Specimen S1:

Load-frequency: 0.2 Hz;

Constant-amplitude-sinusoidal load: 200AX + 45IPB + 2OPB(kN) and $R = \frac{\sigma_{\min}}{\sigma_{\max}} = 0$;

Material constant: $C = 1.427 \times 10^{-12} \text{ (m/cycle)(MPa} \cdot \text{m}^{1/2})^{-3.523}$ and $m = 3.523$;

For Specimen S2:

Load-frequency: 0.2 Hz;

Constant-amplitude-sinusoidal load: 200AX - 45IPB(kN) and $R = \frac{\sigma_{\min}}{\sigma_{\max}} = 0$;

Material constant: $C = 1.427 \times 10^{-12} \text{ (m/cycle)(MPa} \cdot \text{m}^{1/2})^{-3.523}$ and $m = 3.523$;

The initial crack and terminal crack depths used for the residual life prediction for the two specimens is listed in Table 6.1.

Figs. 6.21 and 6.22 compare the residual fatigue life results obtained by using the mesh SOLID_CR with different crack surface (measured, constant $\omega = 0^\circ, -5^\circ, -10^\circ, -15^\circ$ for Specimen S1 and $\omega = 0^\circ, 5^\circ, 10^\circ, 15^\circ$ for Specimen S2) against the actual life recorded during the fatigue tests. The values of a'_{initial} and a'_{final} employed are the values corresponding to the range of a' used in various FE models for the estimation of SIF values in Sections 6.4.3 and 6.4.4. As expected, the numerical models based on measured geometry give the most accurate prediction for both specimens.

For Specimen S1, all the models gave conservative predictions when compared with the actual measurements. In addition, the FE model with the crack surface angle $\omega = 0^\circ$ is able to give prediction more conservative than the exact model. For Specimen S2, as the SIF predictions from all constant angle crack surface model are unconservative in the initiation phase of the crack propagation, the prediction of the residual life in the range $a'/t_0 \leq 0.6$ are also unconservative. As the evaluation of the residual life starts from a'_{final} then accumulates through a'_{initial} , a small unconservative difference in the initiation range could lead to a substantial overestimation of the residual life. However, as shown in Fig. 6.22, a slight overestimation of the FE model with the crack surface angle $\omega = 0^\circ$ might be used to predict the residual fatigue for the specimen, provided that a safety factor is introduced.

6.5.4 Conservatism of the life prediction procedure for partially overlapped CHS K-joints

In general, the prediction procedure might contain many uncertainties, for example the uncertainty when HSS is calculated and subsequently the uncertainty when SIF is estimated. If an error of 5% in the HSS analysis is considered normal, the next step determination of SIF can easily add up another 5%. Therefore in practice an error of 10% in the final SIF calculation is considered acceptable. Because the crack growth rate is roughly proportional to the fourth power of ΔK , the relative error in the crack growth prediction could be up to 30%. Taking into account all the errors that can enter the analysis, it is obvious that a substantial safety factor should be used. The safety factor FOS_N (Lee et al. 2007b) is then defined as the ratio between $N_{\text{act}}(a')$, the actual number of cycle recorded for the crack to penetrate from a'_{initial} to a'_{final} , and $N_r(a')$:

$$FOS_N(a') = \frac{N_{\text{act}}(a')}{N_r(a')} \quad (6.24)$$

The conservatism of the predicted residual life can be assessed by the value of the evaluation factor. A value of $FOS_N < 1.0$ indicates that the prediction is not conservative. In practice, a conservative result of FOS_N is preferred. In the unconservative case, a safety factor FSA_N , which is equal to the reverse value of the evaluation factor, might be useful in practical design. The results of FOS_N for the residual life estimation presented in the previous Section are shown in Figs. 6.23 and 6.24 for Specimens S1 and S2 respectively. For Specimen S1, it can be seen that the value of FOS_N indicates a conservative life prediction from all numerical models. For Specimen S2, it is observed that at the initiation phase (up to $a' = 8$ mm) of the crack propagation, the life prediction for all the constant crack surface angle results in a value less than unity. For a more detailed description, the value of FOS_N from the crack surface model with $\omega = 0^\circ$ for both specimens are listed in Tables 6.2 and 6.3. From these tables, it is suggested that a safety factor greater than 1.4 might be used in conjunction with the proposed crack surface model for the estimation of the residual fatigue life for partially overlapped CHS K-joints.

6.6 CONCLUDING REMARKS

In this Chapter, it is shown that the mesh generation technique proposed in Chapter 4 can be considered stable and reliable for further numerical analysis of partially overlapped CHS K-joints with and without crack. The validation procedure employed the numerical models with exact dimensions obtained from the measurements of welding and crack details. It is concluded that the numerical and experimental results reasonably match for both SCF and SIF values. In addition, it is also noted that the available parametric equation by Efthymiou (1985) only gives the maximum SCF value but not the location of the HSS. Depending on the load case, the parametric values can either be conservative or unconservative. Furthermore, several numerical models are studied. It is recommended that in case actual measurement is not available, the mesh with welding details (SOLID_1W) could be used for SCF prediction and the mesh with crack details (SOLID_CR) with $\omega = 0$ could be used for SIF prediction. It is also concluded that the numerical models with crack could be used to predict the residual life of a partially overlapped CHS K-joints with existed crack, provided that a safety factor greater than 1.4 is used.

Parameters (mm, degree)								
	R_0	$R_1 = R_2$	t_0	$t_1 = t_2$	$\theta_1 = \theta_2$	L_C	L_T	L_B
S1	136.50	122.25	25.0	19.1	45	5840	2446	2100
S2	136.50	122.25	25.0	20.0	45	5840	2446	2100

Fig. 6. 1. General input parameters for the numerical models

(a) SCF on the through brace

(b) SCF on the chord

Fig. 6. 2. Comparison of experimental and numerical SCF values for Specimen S1 under AX loading

(a) SCF on the through brace

(b) SCF on the chord

Fig. 6. 3. Comparison of experimental and numerical SCF values for Specimen S1 under IPB loading

(a) SCF on the through brace

(b) SCF on the chord

Fig. 6. 4. Comparison of experimental and numerical SCF values for Specimen S1 under OPB loading

(a) SCF on the through brace

(b) SCF on the chord

Fig. 6. 5. Comparison of experimental and numerical SCF values for Specimen S1 under combined loading (100AX+45IPB+2OPB kN)

(a) overall deformed shape

(b) HSS distribution around Weld 1

Fig. 6. 6. Visualization of stress distribution in Specimen 1 under combined loading

(a) SCF on the overlapped brace

(b) SCF on the through brace

Fig. 6. 7. Comparison of experimental and numerical SCF values for Specimen S2 under AX loading

(a) SCF on the overlapped brace

(b) SCF on the through brace

Fig. 6. 8. Comparison of experimental and numerical SCF values for Specimen S2 under IPB loading

(a) SCF on the overlapped brace

(b) SCF on the through brace

Fig. 6. 9. Comparison of experimental and numerical SCF values for Specimen S2 under combined loading (100AX-45IPB)

(a) overall deformed shape

(b) HSS distribution around Weld 3

Fig. 6. 10. Visualization of stress distribution in Specimen 2 under combined loading

(c) Overall displacement

(b) Crack opening displacement

Fig. 6. 11. Deformed shape of Specimen S1 with surface crack under combined loading

Fig. 6. 12. SIF along the crack front for Specimen S1 ($a' = 6.48\text{mm}$)

Fig. 6. 13. Comparison of SIF between experimental data and measured numerical model for Specimen S1

(a) Overall displacement

(b) Crack opening displacement

Fig. 6. 14. Deformed shape of Specimen S2 with surface crack under combined loading

Fig. 6. 15. SIF along the crack front for Specimen S2 ($a' = 13.83\text{mm}$)

Fig. 6. 16. Comparison of SIF between experimental data and measured numerical model for Specimen S2

Fig. 6. 17. Comparison of SIF from different numerical models for S1

Fig. 6. 18. Comparison of SIF from different numerical models for S2

Fig. 6. 19. Paris' law

Fig. 6. 20. Numerical integration

Fig. 6. 21. Prediction of residual fatigue life of Specimen S1

Fig. 6. 22. Prediction of residual fatigue life of Specimen S2

Fig. 6. 23. $FOS_N(a')$ for the prediction of residual fatigue life of Specimen S1

Fig. 6. 24. $FOS_N(a')$ for the prediction of residual fatigue life of Specimen S2

Table 6. 1. Crack profiles used in the FE models

Crack profile	α' (mm)	l_{cr1} (mm)	l_{cr2} (mm)	l (mm)
Specimen S1 (AX 200 + IPB 45 + OPB 2) kN				
1	2.41	31.54	38.79	70.33
2	3.72	32.88	40.44	73.32
3	6.48	36.45	44.83	81.28
4	8.24	46.20	56.83	103.03
5	10.12	47.68	58.65	106.33
6	12.20	49.67	61.09	110.76
7	13.75	51.03	62.77	113.80
8	14.51	53.24	65.49	118.73
9	16.20	53.67	66.01	119.68
Specimen S2 (AX 200 - IPB 45) kN				
1	3.32	9.12	10.63	19.75
2	5.01	13.78	16.05	29.83
3	7.65	21.03	24.50	45.53
4	9.39	25.82	30.08	55.92
5	12.22	33.60	39.14	72.74
6	13.83	38.01	44.28	82.29
7	16.05	44.12	51.04	95.52
8	18.57	51.04	59.46	110.5

Table 6. 2. Life prediction from the proposed numerical model for Specimen S1

$a'_{initial}$ (mm)	a'_{final} (mm)	ΔK (MPa $\times$ m ^{1/2})	$N_r(a')$ (cycle)	$N_{act}(a')$ (cycle)	$FOS_N(a')$
2.407	16.197	29.56	25737	36218	1.41
3.725	16.197	31.83	20351	31645	1.55
6.475	16.197	36.62	12475	25092	2.01
8.232	16.197	40.32	9209	20352	2.21
10.123	16.197	42.43	6525	15454	2.37
12.205	16.197	45.59	4136	10399	2.51
13.752	16.197	46.11	2614	7487	2.86
14.516	16.197	45.31	1855	5889	3.18

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

Table 6. 3. Life prediction from the proposed numerical model for Specimen S2

$a'_{initial}$ (mm)	a'_{final} (mm)	ΔK (MPa $\times$ m ^{1/2})	$N_r(a')$ (cycle)	$N_{act}(a')$ (cycle)	$FOS_N(a')$
3.320	17.570	23.04	59423	43023	0.72
5.010	17.570	24.85	42853	39423	0.92
7.650	17.570	28.97	25080	30602	1.22
9.390	17.570	32.19	17793	26102	1.47
12.220	17.570	36.63	9886	19081	1.93
13.830	17.570	37.96	6601	17281	2.62
16.050	17.570	39.14	2573	13281	5.16

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 7

CONCLUSIONS AND RECOMMENDATIONS

7.1 INTRODUCTION

In this study, a novel and consistent geometrical model and mesh generation technique is proposed for partially overlapped CHS K-joints with and without crack. For the mesh without crack, the geometrical model of welding details is developed both on the chord and brace sides and a general algorithm is developed for the determination of the intersection points. For the mesh with crack, a general model for the inclined crack surface and an unsymmetrical crack front are proposed. Based on the developed geometrical model, a new mesh modelling method is proposed for partially overlapped CHS K-joints. The mesh generator is constructed step-by-step, therefore it is able to produce several kinds of meshes such as surface meshes, solid meshes, meshes with or without welding and crack details. The application range of the mesh generator is extended for special cases of identical chord and braces dimensions as well as large overlapped percentage.

In the experimental program, two full-scale partially overlapped CHS K-joints were tested under cyclic combined loads in order to gather more information about the fatigue performance of the joints, as well as to evaluate the surface crack model proposed. The weld profile, shape and surface of the crack of the two full-scale specimens were measured. The measured SCF, SIF and the fatigue life of the specimens are compared with numerical values obtained from the FE models with exact geometry. In addition, several FE models for the assessment of SCF and SIF values are presented for comparison and consideration. A numerical model is proposed for the practical assessment of SCF, SIF and the remaining life of partially overlapped CHS K-joints.

7.2 CONCLUSIONS

7.2.1 Geometrical modelling and mesh generation of partially overlapped CHS K-joints

In the development of the geometrical model of the weld, a new approach is adopted such that the cut off effect with large β ratio can be catered for. The weld model also includes a more detailed description of the weld thickness on the brace side. In order to apply the geometrical modelling of the welding details for the case of multiple intersections, an algorithm is developed for the determination of the theoretical and actual intersection points. For the mesh with crack, a general model for the inclined crack surface and a generally unsymmetrical crack front are proposed.

The mesh of a partially overlapped CHS K-joint is first created in the form of a surface mesh, which is then converted into a solid mesh. The welding and crack details can be added subsequently. To ensure continuous element connectivity among different meshes, different kinds of element are used. The mesh generator is able to handle a wide range of parameters such as special cases of identical chord and braces dimensions and large overlapped percentage. Most importantly, it is able to generate a solid mesh with welding details and surface crack of any length and locates at either sides of the joint intersection. The proposed modelling procedure is flexible such that it can be immediately applied to partially overlapped N-joints.

7.2.2 Experimental studies of full scaled specimens

In the experimental study, two full-scale partially overlapped CHS K-joints were tested under cyclic combined loads. The two partially overlapped CHS K-joint specimens were designed such that overlapping is essential. In addition, the dimensions of the specimens are chosen to study the difference in HSS and fatigue crack locations on a particular specimen once the loading reverses. It is interesting to note that the fatigue test a certain combination of loading was able to cause the crack to occur along the brace side rather than on the chord of the weld. When compared with the existing S-N curve for tubular joints, the fatigue life of Specimen S2 shows unconservative value if the number of cycles N_f when the crack first reach the full thickness of the CHS is used. Other than that, the present S-N curves are conservative.

7.2.3 Numerical analysis for partially overlapped CHS K-joints

The proposed mesh generation technique is validated against the experimental results by using numerical models with measured dimensions. For the estimation of SCF values, it is observed that the trend and value of the numerical results are conservative especially on the heel and saddle areas. However, the prediction of the SCF is not conservative near the intersection points. It might be due to the complex notch effect of the actual weld that has not been catered for in the numerical models. It is also noted that the available parametric equation by Efthymiou (1985) only gives the maximum SCF value, which might predict either conservative or unconservative values of SCF depending on the load case considered. Furthermore, it can be observed that both the trend and values of the numerical SIFs match well with the experimental results. The maximum relative difference of the SIF results from experimental and numerical analyses is less than 10%. In addition, the values of K_e at the deepest points are higher than that at the two crack tips. This observation might also be used to explain for the small ratio between the crack length and depth.

After being validated, the mesh modelling technique was used to generate several numerical models. It is concluded that the numerical results obtained from these models can be used for the practical evaluation of SCF and SIF values for partially overlapped CHS K-joints. It is recommended that in case actual measurement is not available, the solid mesh with welding details, mesh SOLID_1W, is used for SCF prediction and the mesh SOLID_CR with the surface crack angle $\omega = 0$ is used for SIF prediction.

7.2.4 Residual life prediction for joint with existing crack

Once accurate values of SIF are obtained by the numerical analysis, a residual life prediction procedure for partially overlapped CHS K-joints is proposed by rearranging and integrating the Paris' law equation. The residual life, $N_r(a')$ which is the number of load cycles for the crack to penetrate from a depth a' to a'_{final} can be estimated. The procedure is illustrated using the two full-scale test results. Finally, it is concluded that the numerical models with crack together with the residual life prediction procedure can be used to predict the remaining life of a partially overlapped CHS K-joints with existed crack, provided that a safety factor greater than 1.4 is used.

7.3 RECOMMENDATIONS FOR FURTHER STUDY

1. During the mesh generation step, it is assumed that the number of segments on the brace and chord sides along a weld path are equal. The weld path is therefore constructed as a structured mesh. By using this approach, the element quality for the weld path can be considered satisfactory when the sizes of the two braces are almost similar and the percentage of overlapping is not large. Otherwise, the element along the weld path might be distorted. Therefore, it is recommended that in future study, the number of segments along a weld path might be different on the chord and brace sides and/or an unstructured mesh might be developed in this location. This first consideration might be fulfilled by applying a tie constraint between the weld path and the chord/brace members. The tie constraint might help enforce exactly equal displacement, and therefore stress, between the involved components. In addition, numerical effort can be spent to model the accurate crack surface. It is because the actual crack surface does not only curve along the crack length as assumed in the current program, but also curve along the crack depth direction. The crack surface is being assumed flat in the crack depth direction through the use of one element layer in the current geometrical model and mesh generation program. In future research, more layers of elements might be added through the thickness of the crack block to simulate the curved crack surface. The element connectivity between the inner crack ring and other transformation blocks might be changed accordingly.
2. The automatic mesh generator is proposed for the extensive parametric fatigue studies on SCF and SIF values of partially overlapped CHS K-joints. The SCF and SIF results might be used to intensively investigate the fatigue behaviour of the joint depending on the geometrical and loading parameters. In addition, from the database, the relationship between parameters might be established, the importance of individual parameters might be assessed, and optimum combination of parameters might be proposed for the practical design of partially overlapped CHS K-joints. In addition, it has been demonstrated in the fatigue test program that under certain geometrical and loading combinations, the fatigue crack is able to occur at unconventional position along the weld path. It is suggested that fatigue

tests are conducted in conjunction with the behavioural study to investigate the possibility for crack to occur and propagate at the common area.

3. The geometrical modelling and automatic mesh generator can be directly applied for the fatigue studies of partially overlapped CHS N-joints. Besides, with additional numerical effort, the generation of solid mesh with crack for partially overlapped CHS KK-joints can be achieved. Further studies of fatigue test for the partially overlapped CHS N-joints and KK-joint geometry are highly recommended. It is because for complex joint, HSS and fatigue cracks are able to occur at unconventional positions. It is necessary to further investigate whether the conclusions drawn from the partially overlapped K-joints can be applied to the N-joints or KK-joints.

REFERENCES

1. ABABUTT (1989), User Manual, AEA Technology, Risley, Warrington, UK.
2. ABACRACK (1989), User Manual, AEA Technology, Risley, Warrington, UK.
3. ABAMAP (1989), User Manual, AEA Technology, Risley, Warrington, UK.
4. ABAQUS (2006), User Manual (Ver. 6.5), Hibbit, Karlsson and Sorensen Inc., USA.
5. American Petroleum Institute (2000), Recommended Practice for Planning, Designing and Constructing Fixed Offshore Platforms, API RP2A.
6. American Welding Society, (1996), ANSI/AWS D1.1-96 Structural Welding Code-Steel, Miami, USA.
7. ANSYS (1995), User Manual, Swanson Analysis System Inc.
8. API RP 579, (2000), Fitness-for-Service, American Petroleum Institute, Washington, USA.
9. 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.
10. Barsom, J.M., and Novak, S.R. (1977). Subcritical crack growth in steel bridge members, NCHRP Report 181, Transportation Research Board, National Research Council.
11. Barsoum, R.S., (1976), "On the use of isoparametric finite elements in linear fracture mechanics", International Journal of Numerical Methods in Engineering, Vol. 10, No. 1, pp. 25-37.
12. Billington, C. J., and Lalani, M., (1987), Recent Research and Advances in the Design of Tubular Joints. International Conference on Steel and Aluminium Structures - Steel Structures: Advances, Design and Construction, South Glamorgan, pp. 190-209.

13. BOMEL (1992), "Joint industry funded program: analytical and experimental investigation of the behaviour of tubular frames", Final Report, Maidenhead, UK.
14. 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.
15. 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.
16. BS 7910 (1999), Guide on Methods for Assessing the Acceptability of Flaws in Fusion Welded Structures, British Standard Institution, London, UK.
17. Cao, J.J., Yang, G.J., and Packer, J.A., (1997), "FE mesh generation for circular tubular joints with and without cracks", Proceedings of the 7th International Offshore and Polar Engineering Conference, Honolulu, USA, Vol. IV, pp. 98-105.
18. Cao, J.J., Yang, G.J., and Packer, J.A., and Burdekin, F.M., (1998), "Crack modelling in FE analysis of circular tubular joints", Engineering Fracture Mechanics, Vol. 61, pp. 537-553.
19. Chang, E., and Dover, W.D., (1999a), "Parametric equations to predict stress distribution along the intersection of tubular X and DT joints", International Journal of Fatigue, Vol. 21, No. 4, pp. 619-635.
20. Chang, E., and Dover, W.D., (1999b), "Prediction of stress distributions along the intersection of tubular X and DT joints", International Journal of Fatigue, Vol. 21, No. 4, pp. 361-381.
21. Chiew, S.P., Lee, C.K., Lie, S.T. and Ji, H.L., (2007), "Fatigue Behaviors of Square-to-Square Hollow Section T-joint with Corner Crack I: Experimental Studies", International Journal of Engineering Fracture Mechanics, Vol. 74, pp. 703-720.
22. Chiew, S.P., Lie, S.T., Lee, C.K., and Huang, Z.W., (2004), "Fatigue performance of cracked tubular T joints under combined loads. I: Experimental", Journal of Structural Engineering, Vol. 130, No.4, pp. 562-571.

23. Chiew, S.P., Soh, C.K., and Wu, N.W., (1999), "Experimental and numerical stress analysis of tubular XT joints", Journal of Structural Engineering, Vol. 125, No. 11, pp. 1239-1248.
24. Chiew, S.P., Soh, C.K., and Wu, N.W., (2000a), "Experimental and numerical stress analysis of tubular XX joints", Journal of Structural Engineering, Vol. 126, No. 11, pp. 1331-1338.
25. Chiew, S.P., Soh, C.K., and Wu, N.W., (2000b), "General SCF design equation for steel multiplanar tubular XX joints", International Journal of Fatigue, Vol. 22, pp.283-293.
26. Chiew, S.P., Soh, C.K., Soh, A.K., Fung, T.C., and Lee, W.M., (1996), "SCF design equations for steel mutiplanar tubular XT joints", The Structural Engineer, Vol. 74, No. 13, pp. 215-221.
27. 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.
28. Connolly, M.P., Hellier, A.K., Dover, W.D., and Sutomo, J., (1990), "A parametric study of the ratio of bending to membrane stress in tubular Y and T-joints", International Journal of Fatigue, Vol. 12, No 1, pp. 3-11.
29. Cotrell, A.H., (1961), "Theoretical aspect of radiation damage and brittle fracture in steel pressure vessels", ISI Special report No. 69, Iron and Steel Institute, UK.
30. Department of Energy (DEn), (1993), "Background to New Fatigue Design Guidance for Steel Joints in Offshore Structures," Internal Report, Department of Energy, London, UK.
31. Department of Energy, DEn (1993), "Background to new fatigue design guidance for steel welded joint in offshore structures", Report of the DEn, Guidance Notes Revision Drafting Panel, Her Majesty's Stationery Office.
32. Dexter, E.M., and Lee, M.M.K., (1999a), "Static strength of axially loaded tubular K joints. I: Behaviour", Journal of Structural Engineering, Vol. 125, No. 2, pp.194-201.

33. Dexter, E.M., and Lee, M.M.K., (1999b), "Static strength of axially loaded tubular K joints. I: Ultimate capacity", Journal of Structural Engineering, Vol. 125, No. 2, pp.202-210.
34. Dexter, E.M., Lee, M.M.K., and Kirkwood, M.G., (1996), "POCHS K-joints in circular hollow sections under axial loading (an investigation of the factors affecting the static strength using numerical modelling)", Journal of Offshore Mechanics and Arctic Engineering, Vol. 118, pp.53-61.
35. Dharmavasan, S., and Seneviratne, L.D., (1986), "Stress analysis of POCHS K-joints", Fatigue and Crack Growth in Offshore Structures, IMechE 1986-2, pp. 17-30.
36. 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.
37. 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.
38. Dowling, N.E. (1999), Mechanical Behaviour of Materials, 3rd ed., Prentice Hall, NJ
39. Du, Z.Z. and Hancock J.W., (1989), "Stress Intensity Factors of Curved Semi-Elliptical Cracks in a Tubular Welded Joint Using Line Springs and 3D Finite Elements", Journal of Pressure Vessels Technology, Vol. 3, pp. 257-265.
40. Dyer, A.P., (1996), "Yielding of steel sheets containing slits", Journal of Mechanics and Physics of Solids, Vol. 8, No. 2, pp. 100-104.
41. EC3, (2005), "Design of Steel Structures - Part 1.1: General Rules and Rules for Buildings," ENV 2005-1-1, Eurocode 3, European Committee for Standardisation (CEN).
42. Efthymiou, M., and Durkin, S., (1985), "Stress concentrations in T/Y and gap/overlap K-joints", Proceedings of the conference of behaviour of offshore structures, Elsevier Science Publishers, Amsterdam, pp. 429-440.
43. 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

44. Foreman, R.G., Keary, V.E., and Engle, R.M., (1967), "Numerical analysis of crack propagation in cyclic-loaded structures", Journal of Basic Engineering, Vol. 89, pp.459-464.
45. Fung, T.C, Soh, C.K., Gho, W.M., and Qin, F., (2001a), "Ultimate capacity of completely overlapped tubular joints I. An experimental investigation", Journal of Constructional Steel Research, Vol. 57, No. 3, pp. 855-880.
46. Fung, T.C, Soh, C.K., Gho, W.M., and Qin, F., (2001b), "Ultimate capacity of completely overlapped tubular joints II. Behavioural study", Journal of Constructional Steel Research, Vol. 57, No. 8, pp. 881-906.
47. Halliburton (2004), http://www.halliburton.com/oil_gas/sd0953.jsp
48. 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.
49. Healy, B.E., (1994), "A Numerical Investigation into the Capacity of Overlapped Circular K-Joints", Proceeding of 6th International Symposium on Tubular Structures, Melbourne, Australia, pp. 563-571.
50. Healy, B.E., and Buitrago, J., (1994), "Extrapolation procedures for determining SCFs in mid-surface tubular joint models", Conference of Tubular Structures VI, pp. 651-659.
51. Hellier, A.K., Connolly, M.P., and Dover, W.D., (1990a), "Stress concentration factors for tubular Y and T joints", International Journal of Fatigue, Vol. 12, No.1, pp. 13-23.
52. Hellier, A.K., Connolly, M.P., Kare, R.F. and Dover, W.D., (1990b), "Prediction of the stress distribution in tubular Y and T joints", International Journal of Fatigue, Vol. 12, No.1, pp. 25-33.
53. Herion, S., Mang, F. and Puthli, R., (1996), "Parametric study on multiplanar K joints with gap made of circular hollow sections by means of the finite element method", Proceedings of the 6th International Offshore and Polar Engineering Conference, Los Angeles, USA, Vol. IV, pp. 68-73.
54. Hertzberg, R. W., (1996), Deformation and Fracture Mechanics of Engineering Materials, 4th ed., John Wiley & Sons.

55. Holdbrook, S.J., (1980), "The application of linear fracture mechanics to fatigue crack growth in tubular welded joints", PhD Thesis, University College London.
56. HSE, (1993), Offshore Installations: Guidance on design, construction and certification, Consolidated Edition, HSE, HMSO, London, UK.
57. 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.
58. Huang, Z.W., (2003), "Stress intensity of cracked tubular T- and Y- joints under complex loads", PhD Thesis, CEE, NTU, Singapore.
59. Ingraffea, A.R., and Manu, C. (1980), "Stress intensity factor computation in three dimensions with quarter-point elements", International Journal for Numerical Methods in Engineering, Vol. 15, pp.1427-1445.
60. International Institute of Welding (IIW) (1999), Recommended Fatigue Design Procedure for Welded Hollow Section Joints, IIW Doc. XV-1035-99, XV-E-99-251, XIII-1804-99.
61. Irvine, N. M., (1981), "Review of stress analysis technique used in UKOSRP", Proceedings of Conference on Fatigue in Offshore Structural Steels, Institute of Civil Engineers, UK.
62. Irwin G. R., (1958), Handbuch der physic, Fracture, Vol. 6, pp. 551-590.
63. Ji, H.L., (2006), "Fatigue behaviour of square-to-square hollow section T-joints with corner cracks", PhD Thesis, CEE, NTU, Singapore.
64. Karamanos, S.A., Romeijn, A. and Wardenier, J., (1997), "Stress concentrations and joint flexibility effects in multi-planar welded tubular connection for fatigue design", CIDECT Report 7R-17/98, Delft University of Technology.
65. Kuang, J.G., Potvin, A.B., Leick, R.D., and Kahlich, J.L., (1978), "Stress concentration in tubular joints", Society of Petroleum Engineering, USA, pp. 287-299.
66. Kurobane, Y., Makino, Y., and Ochi, K., (1984), "Ultimate resistance of

- unstiffened tubular joints”, Journal of Structural Engineering, Vol. 110, No. 2, pp. 385-400.
67. Kurobane, Y., Makino, Y., and Ochi, K., (1986), “Local buckling of braces in tubular K-joints”, Thin-Walled Structures, Vol. 4, No. 1, pp. 23-40.
  68. Lalani, M., and Forsyth, P., (1987), “The fatigue behaviour of overlapping joints”, Steel in Marine Structures, Vol. 8, Elsevier, the Netherlands, pp. 431-443.
  69. Lee, C.K. (1999). “Automatic adaptive mesh generation using metric advancing front approach”, Engineering Computations, 16(2), 230-263.
  70. Lee, C.K., Chiew, S.P., Lie, S.T. and Ji, H.L., (2007), “Fatigue Behaviors of Square-to-Square Hollow Section T-joint with Corner Crack II: Numerical Modeling”, International Journal of Engineering Fracture Mechanics, Vol. 74, pp. 721-738.
  71. Lee, C.K., Lie, S.T., and Wong, S.M., (1999) “Model and mesh generation of cracked tubular joints”, Proceedings of the Europe Conference on Computational Mechanics, pp. 182-183.
  72. Lee, C.K., Lie, S.T., Chiew, S.P., Sopha, T. and Nguyen, T.B.N. (2007). Experimental Studies on Fatigue Behaviour of Partially Overlapped CHS K-joints, International Maritime-Port Technology and Development Conference, Singapore, pp. 280-285.
  73. Lee, C.K., Wong, S.M., and Lie, S.T., (2000), “On increasing the order and density of 3D finite element meshes”, Communication in Numerical Methods in Engineering, Vol.17, No.1, pp.55-68.
  74. Lee, M.M.K., (1999), “Strength, stress and fracture analyses of offshore tubular joints using finite elements”, Journal of Constructional Steel Research, Vol. 51, pp. 265-286.
  75. Lie, S.T., Chiew, S.P., Lee, C.K. and Huang, Z.W., (2004), “Fatigue performance of cracked tubular T joints under combined loads. II: Numerical”, Journal of Structural Engineering, Vol. 130, No.4, pp. 562-571.
  76. 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.
77. Maosheng, C., Shan, W., and Shaofu, L., (1992), "Crack growth and fatigue life estimation in welded T- tubular joints based on fracture mechanics", Proceeding of 5th International Symposium on Tubular Structures, pp. 351-358.
78. McEvily, A.J., (1988), "Rate of growth of fatigue cracks calculated from the theory of infinitesimal dislocations distributed on a plane", International Journal of Fracture Mechanics, Vol. 2, pp.460-467.
79. Monahan, C.C., and Dover W.D., (1995), "Early fatigue crack shape development in welded tubular joints", Proceedings of the 12th Offshore Mechanics and Arctic Engineering Conference, pp. 223-231.
80. Morgan, M.R. and Lee, M.M.K., (1997), "New parametric equations for stress concentration factors in tubular K joints under balanced axial loading", International Journal of Fatigue, Vol. 19, No. 4, pp. 309-317.
81. Morgan, M.R. and Lee, M.M.K., (1998a), "Parametric equations for distributions of stress concentration factor in tubular K-joints under out-of-plane moment loading", International Journal of Fatigue, Vol. 20, No.6, pp. 449-461.
82. Morgan, M.R. and Lee, M.M.K., (1998b), "Stress concentration factor in tubular K-joints under in-plane moment loading", Journal of Structural Engineering, Vol. 124, No.4, pp. 382-390.
83. Pang, H.L.J., and Lee C.W., (1995), "Three dimensional finite element analysis of a tubular T joint under combined axial and bending loading", International Journal of Fatigue, Vol. 17. No. 5, pp. 313-320.
84. Pang, N.L.D., (2007), Fatigue Behaviour of Dragline Tubular Structures, PhD thesis, Monash University, Australia.
85. Paris, P.C. and Erdogan, F., (1963), "A Critical Analysis of Crack Propagation laws," Journal of Basic Engineering, 85(4), 528-534.
86. Paris. P. C., Gomez, and Anderson, (1972) "Extensive Study of Low Fatigue Crack Growth Rates in A533 and A508 Steels" Proceedings of the 1971 National Symposium on Fracture Mechanics, Part I, ASTM STP 513, pp. 141-

- 176.
87. PATRAN (1993), User Manual, PDA Engineering.
88. PMBSHELL (1992) User Manual: A Special Purpose Finite Element Program for Stress Analysis of Tubular Joints, Chang, T.Y.P.
89. PRETUBE (1991), User's Manual. Veritas Sesam System, Det Norske Veritas (DNV) Industry Ltd., Norway.
90. 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.
91. Qian, R., Zheng, X. and Xue, J., (1992), "Propagation of fatigue crack at hot spots of tubular T-joints", Tubular Structures V, pp. 359-363.
92. Raju, I.S., and Newman, J.C., (1979), "Stress intensity factors for a wide range of semi-elliptical surface cracks in finite-thickness plates", Engineering Fracture Mechanics, Vol. 11, pp. 817-829.
93. Rhee, H.C., 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", Proceedings of the 10th Offshore Mechanics and Arctic Engineering Conference, Stavanger, Norway. Vol. III-B, pp. 441-452.
94. Rice, J. R., (1968). Fracture, ed. Liebiwitz, Academic Press, Vol II, 191-311.
95. Shao, Y.B., (2004), "Fatigue Behaviour of Uniplanar CHS Gap K-joints under Axial and In-plane Bending Loads", PhD Thesis, CEE, NTU, Singapore.
96. 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, Vol. 55, No.2, pp.299-316.
97. Smedley, P. and Fisher, P., (1991), "Stress concentration factor for simple tubular joints", Proceedings of the 1st International Offshore and Polar Engineering Conference, Edinburgh, UK, pp. 475-483.

98. Stacey, A., and Sharp, J.V., (1997), "Fatigue damage in offshore structures – cause, detection and repair", Proceedings of the 8th International Conference on the Behavior of Offshore Structures, Delft University of Technology.
99. 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.
100. Suen, D.S., and Wu, J.H., (1993), "Computer-aided design of welded tubular connections", Proceedings of the 3rd International Offshore and Polar Engineering Conference, Singapore, Vol. IV, pp. 393-400.
101. Tizani, W.M.K., Yusurf, K.O., Davies, G. and Smith, N.J., (1996), "A knowledge based system to support joint fabrication decision making at the design stage – case study for CHS trusses", Proceedings of the 7th International Symposium on Tubular Structures, Hungary, pp. 483-489.
102. Tweed, J.H., (1987), "Surface crack growth behaviour of UKOSRP II joints and implications for prediction of remaining life of defective tubular joints", Steel in Marine Structures, Elsevier Science Publisher, Amsterdam.
103. Tweed, J.H., and Freeman J.H., (1987), "Remaining life of defective tubular joints – An assessment based on surface crack growth in tubular joints fatigue tests", Offshore Technology Report, HMSO.
104. 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, Vol. 35, No1, pp.71-115.
105. van Wingerde, A.M., Packer, J.A., and Wardenier, J., (1996), "New guidelines for fatigue design of HSS connections", Journal of Structural Engineering, ASCE Vol. 122, No. 2, pp. 125-132.
106. van Wingerde, A.M., Packer, J.A., and Wardenier, J., (1997) "IIW fatigue rules for tubular joints", IIW International Conference on Performance of Dynamically Loaded Welded Structures, San Francisco, USA, pp. 98-107.
107. van Wingerde, A.M., Packer, J.A., and Wardenier, J., Dutta, D., and Marshall, P.W., (1992), "Proposed revisions for fatigue design of planar welded connections made of hollow structural sections", Proceedings of the 5th International Symposium on Tubular Structures, Nottingham, UK, pp. 663-672.

108. Weertman, J., (1966), "Rate of growth of fatigue cracks calculated from the theory of infinitesimal dislocations distributed on a plane", International Journal of Fracture Mechanics, Vol. 2, pp. 460-467.
109. Wells, A.A., (1961), "Unstable crack propagation in metals: cleavage and fast fracture", Proceedings of crack propagation symposium, Vol.1, pp. 210-230.
110. Westergaard, H. M., (1939), "Bearing pressure and cracks", Journal of Applied Mechanics, Vol. 61, pp. 49-53.
111. Wong, S.M., (2000), Model and mesh generation of cracked Y-tubular joints for SIF and SCF studies, MEng Thesis, CEE, NTU, Singapore.
112. Wordsworth, A.C. and Smedley, G.P., (1978), "Stress concentration at unstiffened tubular joints", Proceedings European Offshore Steel Research Seminar, Cambridge, UK, paper 26.
113. Wordsworth, A.C., (1981), "Stress concentrations at unstiffened tubular joints", European Offshore Research Seminar, Cambridge, UK, paper 31.
114. Wylde, J.G., (1981), "Modes of fatigue crack development and stiffness measurement in welded tubular joints", Fatigue in Offshore Structural Steel, Thomas Telford, London, pp. 79-88.
115. Wikipedia on Oil Platform, (2008) http://en.wikipedia.org/wiki/oil_platform
116. Xu, Q.X., (2006), "Analysis of thin-walled structural joints using 3D solid element", PhD Thesis, CEE, NTU, Singapore.
117. Yang, G.J., (1996), "Effect of mixed mode fracture on ultimate strength of cracked tubular joints", PhD Thesis, Department of Civil and Structural Engineering, UMIST, UK.
118. Yazdani, N., and Albrecht, P. (1989). "Crack growth rate for structural steel in air and aqueous environment", Engineering Fracture Mechanics, Vol. 32, pp. 997-1007.
119. 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.

PUBLICATIONS

Journal papers:

1. Lee, C.K., Chiew, S.P., Lie, S.T., and Nguyen, T.B.N., (2008), Adaptive Mesh Generation Procedures for Thin-walled Tubular Structures, *Journal of Finite Element in Analysis and Design*, (under review).
2. Sopha, T. and Nguyen, T.B.N., Chiew, S.P., Lee, C.K. and Lie, S.T., (2008), Stress Analysis and Fatigue Test of Partially Overlapped CHS K-joints, *International Journal of Advanced Steel Construction*, Vol.4, No.2, pp.134-147.
3. Sopha, T. and Nguyen, T.B.N., Chiew, S.P., Lee, C.K. and Lie, S.T., (2008), Experimental Studies on Stress Concentration Factors for Partially Overlapped Circular Hollow Section K-Joints, *International Journal of Advanced Steel Construction* (in press).

Conference papers:

1. Chiew, S.P., Lee, C.K., Lie, S.T. and Nguyen, T.B.N., (2006) Geometrical Modelling of Partially Overlapped CHS K-joints, *The 8th International Conference on Steel, Space and Composite Structures*, Malaysia, pp. 425-435.
2. Lee, C.K., Lie, S.T., Chiew, S.P., Sopha, T. and Nguyen, T.B.N., (2006) An Experimental Study on the Fatigue Behaviour of Partially Overlapped CHS K-joints, *The 8th International Conference on Steel, Space and Composite Structures*, Malaysia, pp. 273-281.
3. Chiew, S.P., Lee, C.K., Lie, S.T., Nguyen, T.B.N. and Sopha, T., (2007) Fatigue Behaviour of Partially Overlapped CHS K-joints, *The Pacific Structural Steel Conference*, New Zealand, Vol.2, pp. 145-152.
4. Chiew, S.P., Lee, C.K., Lie, S.T., Nguyen, T.B.N. and Sopha, T., (2007) Mesh Generation for Partially Overlapped CHS K-joints under Fatigue Loadings, *International Maritime-Port Technology and Development*

Conference, Singapore, pp. 313-318.

5. Lee, C.K., Lie, S.T., Chiew, S.P., Sopha, T. and Nguyen, T.B.N. (2007) Experimental Studies on Fatigue Behaviour of Partially Overlapped CHS K-joints, *International Maritime-Port Technology and Development Conference*, Singapore, pp. 280-285.
6. Lee, C.K., Lie, S.T., Chiew, S.P., Sopha, T. and Nguyen, T.B.N. (2007) Experimental Studies on Stress Distributions for Partially Overlapped CHS K-joints, *The 9th International Conference on Steel, Space and Composite Structures*, China, pp. 262-272.
7. Lee, C.K., Lie, S.T., Chiew, S.P., Sopha, T. and Nguyen, T.B.N., (2007) On Stress Concentration Factors for Partially Overlapped CHS K-joints by Experimental Studies, *The 5th International Conference on Advance in Steel Structures*, Singapore, pp. 563-574.
8. Chiew, S.P., Lee, C.K., Lie, S.T., and Nguyen, T.B.N., (2008), Fatigue Modelling for Partially Overlapped CHS K-joints with Surface Cracks, *the 12th International Symposium on Tubular Structures*, Shanghai, China.
9. Lee, C.K., Chiew, S.P., Lie, S.T., and Nguyen, T.B.N., (2008), Hierarchical Modeling Tools for Fatigue Analysis of Partially Overlapped CHS K-joints, *the 2nd International Symposium on Advances in Steel and Composite Structures*, Singapore.

APPENDIX A

Fig. A.1. API welding details for tubular connections

Notes:

- 1. The dihedral angle is defined as α .
- 2. "T" is the minimum weld thickness.
- 3. "t" is the thickness of brace.

Fig. A. 1 AWS welding details for complete joint penetration in tubular T-, Y-, K-connections

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

**MEASUREMENT DATA OF WELDING PROFILE FOR
SPECIMENS SS1-SS4**

APPENDIX C

**MEASUREMENT DATA OF WELDING PROFILE FOR
SPECIMENS S1, S2**

APPENDIX B

Fig. B1. Modeled and measured shapes, profile graphs and k-graphs for Specimen SS1R (to be continued)

Fig. B1. Modeled and measured shapes, profile graphs and k -graphs for Specimen SS1R (continued)

Fig. B2. Modeled and measured shapes, profile graphs and k -graphs for Specimen SS1L (to be continued)

Fig. B2. Modeled and measured shapes, profile graphs and k -graphs for Specimen SS1L (continued)

a2) Actual shape

b2) k graph (AB)

a1) Modeled shape

b1) Profile graph (AB)

Fig. B3. Modeled and measured shapes, profile graphs and k -graphs for Specimen SS2R (to be continued)

c2) k -graph (CD)

d2) k -graph (EF)

c1) Profile graph (CD)

d1) Profile graph (EF)

Fig. B3. Modeled and measured shapes, profile graphs and k -graphs for Specimen SS2R (continued)

Fig. B4. Modeled and measured shapes, profile graphs and k -graphs for Specimen SS2L (to be continued)

Fig. B4. Modeled and measured shapes, profile graphs and k -graphs for Specimen SS2L (continued)

Fig. B5. Modeled and measured shapes, profile graphs and *k*-graphs for Specimen SS3R (to be continued)

Fig. B5. Modeled and measured shapes, profile graphs and k -graphs for Specimen SS3R (continued)

Fig. B6. Modeled and measured shapes, profile graphs and *k*-graphs for Specimen SS3L (to be continued)

Fig. B6. Modeled and measured shapes, profile graphs and k -graphs for Specimen SS3L (continued)

Fig. B7. Modeled and measured shapes, profile graphs and k -graphs for Specimen SS4R (to be continued)

Fig. B7. Modeled and measured shapes, profile graphs and k -graphs for Specimen SS4R (continued)

a2) Actual shape

b2) k-graph (AB)

a1) Modeled shape

b1) Profile graph (AB)

Fig. B8. Modeled and measured shapes, profile graphs and k -graphs for Specimen SS4L (to be continued)

Fig. B8. Modeled and measured shapes, profile graphs and k -graphs for Specimen SS4L (continued)

Fig. B9. Modeled and measured weld root paths for weld curve 1 (to be continued)

(e) Specimen SS3L

(f) Specimen SS3R

(g) Specimen SS4L

(h) Specimen SS4R

Fig. B9. Modeled and measured weld root paths for weld curve 1 (continued)

Fig. B10. Modeled and measured weld root paths for weld curve 2 (to be continued)

Fig. B10. Modeled and measured weld root paths for weld curve 2 (continued)

(a) Specimen SS1L

(b) Specimen SS1R

(c) Specimen SS2L

(d) Specimen SS2R

Fig. B11. Modeled and measured weld root paths for weld curve 3 (to be continued)

γ

--- k3 as in Ch3   — Validated k3   x S3R

(e) Specimen S3L

γ

--- k3 as in Ch3   — Validated k3   x S4L

(g) Specimen SS4L

γ

--- k3 as in Ch3   — Validated k3   x S3L

(f) Specimen S3R

γ

--- k3 as in Ch3   — Validated k3   x S4R

(h) Specimen SS4R

Fig. B11. Modeled and measured weld root paths for weld curve 3 (continued)

Fig. C1. Modeled and measured weld paths for weld curve 1, Specimen 1

Fig. C3. Modeled and measured weld paths for weld curve 3, Specimen 1

Fig. C4. Modeled and measured weld paths for weld curve 1, Specimen 2

Fig. C5. Modeled and measured weld paths for weld curve 2, Specimen 2

Fig. C6. Modeled and measured weld paths for weld curve 3, Specimen 2