Fatigue study of partially overlapped circular hollow section K-joints. Part 2 : experimental study and validation of numerical models

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FATIGUE STUDY OF PARTIALLY OVERLAPPED CIRCULAR HOLLOW SECTION K-JOINTS

PART 2: EXPERIMENTAL STUDY AND VALIDATION OF NUMERICAL MODELS

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Abstract

In the second part of this study, the reliability of the geometrical models and the mesh generation procedure developed in Part 1 are validated by comparing the modelling results with full scale tests results. Static tests were applied to study the stress concentration factors while fatigue tests were applied to study the stress intensity factor and the fatigue life of the joints. The results obtained indicated that the uncracked joint model could lead to reliable stress concentration factor estimations while the cracked joint models could lead to conservative stress intensity factor and residual fatigue life predictions close to experimental results.

Keywords: stress concentration factor, stress intensity factors, residual fatigue life, partially overlapped CHS K-joint

1 Introduction

For the fatigue studies of tubular joints, both numerical modelling [1-4] and full scale experimental study are commonly used [5-7]. Experiments could let researchers study the true responses of the joint, but with the disadvantage that they are expensive, time consuming and limited by the equipment available. Such disadvantages become more obvious for complex joint configurations or when complex loadings are applied. To complement the shortcomings of full scale tests, numerical modelling using the finite element method is frequently employed to provide a faster and more economical means of study. In many successful studies [3-8], experimental and numerical studies are carefully combined in the following steps to obtain a thorough understanding on the behavior of the targeted joint type:

- (1) Carry out a few carefully designed full scale or scaled tests on the target joint type.
- (2) Create the corresponding geometrical and finite element models to represent the targeted joint type. The reliability of the models is validated by checking the modelling results against the measurements obtained in the experimental study.
- (3) Conduct a large scale parametric study to cover the dimension ranges that are normally used in practice.
- (4) Summarize the parametric study results into various forms of design equations or charts for practical use.

The above procedure has been successfully employed for the development of many design codes for tubular joints applications. Obviously, the reliability of the design guides generated by the above procedures depends on the correctness and reliability of the geometrical and finite element models used. For the design of tubular joint under fatigue loading, since the numerical modelling results obtained are sensitive to the modelling details, many design codes [9] explicitly outline the proper modelling practice needed. In Part 1 [10], a set of geometrical models and an accompanying set of mesh generation procedures are suggested

for the modelling of partially overlapped circular hollow section (CHS) K-joints. While these models are carefully designed based on studies carried out on full scale joints constructed, it is essential that their validity and reliability must be verified before they could be employed for a large scale parametric study. The main objective of this paper is carry out such verification by comparing the modelling results with those obtained from full scale fatigue tests [7,11]. In addition, efforts are focused to identify those parameters that may critically affect the accuracy of the modelling results. In the next section, a brief outline on the experimental studies conducted on two full scale joints is given. For consistency, all the notations and the names of the intersections and the welding curves adopted in Part 1 [10] are reused in this paper (Table 1, Figs. 1 and 15 of reference [10]). In Section 3 to Section 6, the experimental results obtained from the tests are compared with the numerical results obtained. Finally, conclusions for the present study and potential areas of future research works are presented.

2 Full scale tests of partially overlapped circular hollow section K-joint

In order to obtain the actual responses of partially overlapped CHS K-joint under static and cyclic loadings, two full scale joints were tested [7,11]. As the details experimental set up and testing procedures had been reported in references [7] and [11], only a concise summary is given in this section.

2.1 Test set up and specimens

Two full scale partially overlapped CHS K-joints named as Specimen S1 and Specimen S2 were designed and tested by using the specially designed rig shown in Fig. 1. The dimensions of these specimens are shown in Fig. 2. Their dimensions were selected in such a way that while no eccentricity appears for the overlapped joints, if they were fabricated as gapped joints with a minimum gap distance, the eccentricity of the gapped joints will exceed the allowable limited [9]. Furthermore, the sizes of the CHS sections used are almost identical.

Such configuration was, in fact, deliberately selected to study the effects of the loading on the distribution of the stress concentration factor (SCF) of the joints. The CHS employed to construct the joints are fully complied with the design code [9]. All welding were created to fully comply with the American Welding Society specification [12] with properly grinded weld finishes and the weld quality was checked using ultrasonic technique. During testing, the specimens were fixed at the two ends of the chord and the overlap brace while loadings were applied at the end of the through brace by three mutually perpendicular actuators.

2.2 *Tests conducted*

Both static and fatigue tests were conducted for the two specimens. For static test, the three basic loading cases, namely axial loading (AX) and in-plane bending (IPB) and out-of-plane bending were applied in turn for the study of the SCF and the hot spot stress distributions along the weld toes of the intersection curves. For the fatigue test, combined AX (200kN) and IPB (± 45 kN) sinusoidal constant amplitude cyclic loadings of 0.2Hz were applied. The loading direction of the IPB loading applied to Specimen S1 is exactly opposite to that applied to Specimen S2 (Figs. 2 and 3). Such loadings were specially arranged so that for Specimen S1, a crack was eventually induced along the intersection curve between the chord and the through brace (*Curve 1*). While for Specimen S2, the joint was eventually failed by a crack induced along the intersection curve between the through and the overlap brace (*Curve 3*). The joint were finally broken by the cyclic loading until through thickness cracks were detected. The junction part were cut out and opened up for crack shape measurements.

2.3 *Specimens details and experimental results*

Since the specimens are expensive to construct and the fatigue tests require long duration (up to two weeks) to complete, attention was paid to measure all critical data from the specimens

before, during and after the tests. As the main objective of this paper is to validate the correctness and reliability of the geometrical and the finite element models, only a concise summary of the data measured is given here. Details of the experimental results and conclusions from the static and fatigue tests could be found in references [7] and [11].

2.3.1 Measurements done during fabrication and before the tests

Before the tests were carried out, the following geometrical properties of the specimens were measured.

- (1) All basic dimensions of the sections including the actual the intersection angles and overlapping ratio.
- (2) The actual weld profile of the joints along all the three *Curves 1, 2, and 3*. As the weld thickness is the main parameter in the geometrical model that affects the hot spot stress, the actual values materialized along all weld curves were careful measured.

2.3.2 Measurements done during the tests

In order to validate the reliability of the finite element model, the following responses of the specimens during the static and fatigue tests were recorded.

- (1) Strain distributions along the weld profile. During the static test, the strain values developed when different loadings were applied were recorded by extensive arrays of strain gauges attached along the three weld curves (Fig. 4). As it is expected that complex stress responses will be developed near the junction point, a few arrays of rosettes were installed there. For other locations along the weld profile, either single linear strain gauge or pairs of strain gauges arranged in perpendicular configuration were used [10]. The strain gauges array installed ensured that accurate values of hot spot stress can be extracted by using the extrapolation technique specified in the design code [9]. Additional strain gauges were installed along the through brace sections to recheck the alignment of

the specimen so that the designed loadings were correctly applied. During the fatigue test, all strain gauges were disengaged and no dynamic strain value was recorded.

- (2) Crack profiles development during the fatigue test. By using the strain measurement results obtained from the static test, the peak hot spot stress locations could be located with high accuracy. Therefore, an array of alternating current potential drop [7,11,13] probes was installed at regions where the peak hot spot stress were located. A probe spacing of 10mm was used for both specimens and the crack profiles (Fig. 5) were recorded using a scan interval of 5 minutes (equivalent to 180 cycles of the cyclic loading). Besides defining the shape of the crack surface, the crack profiles measured could also be used to compute the experimental stress intensity factor (SIF) by first retrieving the crack front growth rate from the profile records and then substitute its value into the Paris law equation [4,6,14].

2.3.3 Measurements done after the tests

After the fatigue tests were completed, the junction parts were cut out from the specimens. In order to study the *final* crack surface and crack front shapes, the cracked parts of the two specimens were opened up so that the cracked surfaces were exposed. A simple clay molding procedure [11] was then applied to measure the shape of the crack surface. In particular, the actual variation of the crack surface angle ω (which defines the crack shape in the proposed geometrical model [10]) with the depth of the crack was recorded.

3 Validation of the weld profile model for uncracked joints

As it was expected that the peak hot spot stress was located along the crown of *Weld 1* on the through brace side for Specimen S1 and along the crown of the through brace side of *Weld 3* for Specimen S2, the details of the weld thickness achieved there were carefully analysed.

Fig. 6 plots the modelled and the actual weld thickness (T_w) and the thickness factor (k_{Tw}) along the through brace side of *Weld 1* for Specimen S1. The corresponding thickness and factor along the through brace side of *Weld 3* for specimen S1 is shown in Fig. 7.

In Figs. 6(a) and Fig. 7(a), it was assumed that the measured inner weld paths coincide with the proposed model. It is because the joints could only be opened up along the outer weld path and the actual inner weld thickness achieved could not be measured. Nevertheless, it was shown in Part 1 of this study [10] that the contribution of the inner weld thickness to the total weld thickness is very small and could be neglected. From Figs. 6 and 7, it can be observed that the modelled weld thickness resembled the actual weld shapes and both of them satisfied the American Welding Society [12] and the American Petroleum Institute [15] requirements. More importantly, the actual weld thickness is larger than the modelled thickness (especially at crown heels) which implies that the proposed model is conservative so that the predicted hot spot stress should be higher than the actual value.

From Fig. 6, it is interesting to see that at the saddle of Specimen S1, the outer welding profile along *Weld 1* is not as smooth as in other positions. This was due to the effect of post weld grinding (which was done by a qualified worker to ensure that the finished joint could satisfy the American Welding Society standard) and the relatively inaccessibility of the saddle position. From Fig. 7, along *Weld 3* for Specimen S2, since the weld path is short and is conveniently accessible for grinding, the actual weld profile is smoother.

4 Validation of SCF predictions for uncracked joints

In order to validate the geometrical and finite element models for the uncracked joints, the two tested joints were modelled using the technique presented in Part 1 [10]. In order to investigate the effects of different weld profile models, the following finite element models were created.

- (i) Models based on the measured weld thickness
- (ii) Models based on the proposed weld thickness
- (iii) Models without weld details

All the finite element analyses were conducted by using the general purpose program ABAQUS [16]. In order to conduct valid comparisons with the experimental results, for the models based on the measured and the proposed thicknesses, the hot spot stress were extracted by using the same quadratic extrapolation procedure that was employed along the weld toe for experimental hot spot stress calculation [3,9]. For the models without weld details, similar extrapolation procedure was carried out along all intersection curves.

The finite element mesh generated for the analysis of Specimen S1 is shown in Fig. 8a while Fig. 8b shows the hot spot stress distribution when the specimens is subjected to IPB loading. In order to ensure that the mesh is sufficiently refined to give accurate results, a convergent study using a refined mesh with doubled element density was also employed for the analysis. It was found that both the coarse and the refined meshes gave very similar hot spot stress distributions. Hence, in all the subsequent analysis, only meshes with element density similar to the one shown in Fig. 8 were employed. The corresponding SCF distributions along *Curve 1 (Weld 1)* under the AX and the IPB loading cases are plotted in Figs. 9 and 10, respectively. For Specimen S2, the corresponding mesh and hot spot stress distribution are shown in Fig. 11. The hot spot stress distribution along *Curve 3 (Weld 3)* under the AX and the IPB loading cases are plotted in Figs. 12 and 13, respectively. In all the SCF plots, the measured SCF and the corresponding predictions by using the Efthymiou's equations [1] are also presented. From Figs. 8 to 13, the following observations regarding the accuracies of different weld profile models could be seen.

- (1) For all cases, if one regards the experimental SCF distributions (which are obtained by dividing the experimental hot spot stress by the nominal stresses) are the “exact”

solutions, it is obvious that the finite element model associated with the measured weld profile gave the best prediction (with relative error within 10% in all cases). However, it should be mentioned that this model is of limited practical value as it could only be generated after the joint is constructed and detailed measurements are done.

- (2) In general, both the finite element models with and without weld details successfully reproduced the trend of the experimental SCF distributions. Furthermore, in general, they produced higher (conservative) SCF distributions when compared with the experimental measurements. The only underestimation occurred when it comes close to the intersection point (e.g. Fig. 10a, at distance 300mm to 400mm from heel). This might be due to the complex notch effect of the actual weld that had not been catered for in the geometrical and the numerical models. Nevertheless, as the SCF at that location is the lowest along this point, so such underestimation does not impair the practical value of the models.
- (3) Detail comparison between the SCF distributions predicted by models with and without weld details show that the latter model is much more conservative. For Specimen S1, the relative difference in the SCF prediction between the two models is approximately 23% near the saddle area on the brace side of *Weld 1 (Curve 1)* in the case of AX loading (Fig. 9). By contrast, the model with weld details gave less overestimated SCF values with a maximum relative error of 15%. Similar observations could be seen for the IPB loading case (Fig. 10). For Specimen S2, similar results could be found for both the AX and the IPB loading cases. Hence, it could be concluded that correct modelling details of the weld profile are an important factor that affects the accuracy of the SCF prediction. Furthermore, the model without weld profile detail is too conservative.
- (4) The suggested equations by Efthymiou [1] did not give consistent prediction of the SCF under different loading cases along different curves. For example, for Specimen S1, underestimations are obtained along the chord side for the AX load case (Fig. 9b) and

along the through brace side for the IPB case (Fig. 10a) while overestimations are found for other cases (Figs. 9a and 10b). Similar situations could also be seen for Specimen S2 (e.g. Figs. 12b and 13b). Note that the Efthymiou equations only provide a single value for the maximum SCF and therefore horizontal lines are plotted to represent their predictions. In addition, no result from the Efthymiou equations is plotted for Figs. 12a and 13a as the equations only provide predictions for the chord and the through brace. In conclusion, the SCF results obtained show that the numerical models produced reasonable SCF predictions for the specimens tested. Furthermore, the model with weld details is recommended for SCF prediction.

5 Validations of crack surface geometry and crack front shape

After the fatigue test, the joints were forced open along the cracked surface. In general, the clay mold measurements indicated that for both specimens, the crack surface shape varies along the brace-chord intersections. Furthermore, the crack surfaces are not always perpendicular to the wall of the CHS and are unsymmetrical with respect to the deepest point. The variation of the crack surface angle ω (Fig. 12 of [10]) along the crack surface corresponding to different curved crack depth to the section thickness ratio (a'/t) for the specimens are plotted in Fig. 14. From Fig. 14, it can be observed that:

- (i) In general, ω decreases as the ratio a'/t increases at all sections for both specimens.
- (ii) For Specimen S1, ω is negative at the deepest point and varies within a small range of $[-15^\circ, -5^\circ]$. Away from the deepest point, the range is larger but still within $[-20^\circ, 0^\circ]$.
- (iii) For Specimen S2, ω is positive at the deepest point and again varies within a small range of $[0^\circ, 7^\circ]$. Away from the deepest point, the range is larger but still within $[3^\circ, 12^\circ]$.

Hence, it could be concluded that the variation of the crack surface angle ω is quite small and ω is largely close to zero. For the shape of the final crack, comparisons between the modelled

and the measured shapes in terms of crack length and crack depth are plotted in Fig. 15. It can be seen that although at some positions along the crack length the modelled crack shape curves display discrepancy from the measured ones, the bi-elliptical crack shape is a reasonable approximation to the crack front. Note that the negative value of a' near the left hand side of Fig. 15a was due to a faulty probe which gave erratic data during measurement. In addition, the larger deviation observed near the right hand side of Fig. 15a was in fact due to branching of the crack from the weld toe (Fig. 16).

6 Validations of SIF and residual fatigue life predictions for cracked joints

6.1 Prediction of SIF values

The two partially overlapped CHS K-joints with measured crack geometry are discretized by the mesh generation procedure described in Part 1 [10]. In order to obtain a complete picture of the SIF as the crack developed, a number of models corresponding to different crack depths and crack lengths subjected to the maximum cyclic loading (Fig. 3) were created. The results obtained from these models were then compared with the experimental measurements.

As the mesh generation procedure employed could allow the user to generate solid finite element meshes with crack surface details corresponding to any crack surface angle ω within the range $[-20^\circ, 20^\circ]$, several sets of meshes corresponding to different values of ω were generated. In all these models, ω was assumed to be constant with respect to the ratio a'/t and along the crack length. The choice for a constant crack surface angle is based on the observation in Section 5 that it only varies in a small range along the crack front. Note that in order to investigate the effects of ω on the SIF prediction, all other geometrical parameters such as the length and the depth of the surface crack employed in these models are identical to the measurements obtained from the test (Table 1). Besides the models generated with

different ω values, further finite element models were manually generated which reproduce the exact crack profile from the measured crack shape.

Fig. 17 compares the SIF at the deepest point obtained from the experiments with the finite element models based on measured crack shapes for the two specimens. It could be seen that by combining the suggested mesh generation scheme with the actual crack surface geometry, it is possible to predict the SIF value with good accuracy (maximum relative error <10%).

Fig. 18 compares the SIF at the deepest point predicted by models with different values of ω . For Specimen S1, the range for ω is $[0^\circ, -15^\circ]$. The reason for the use of negative ω is based on the observation that the actual crack surface propagates outward from the *Weld 1* towards the through brace side (Fig. 14a). In contrast, for Specimen S2, the range for ω is $[0^\circ, 15^\circ]$ as the crack surface propagated on the overlap brace side of *Weld 3*. For Specimen S1, it can be seen that the models with smaller absolute value of ω ($\omega = 0^\circ, -5^\circ$) gave SIF values closer to the experimental results during the initiation phase of the crack propagation ($a'/t \leq 0.35$). On the other hand, the finite element models with larger absolute value of ω ($\omega = -10^\circ, -15^\circ$) gave SIF values closer to the experimental values at the end of the crack propagation ($a'/t > 0.40$). 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 (Fig. 14a). However, in general, good correlation with test results were obtained for all models, including the model with $\omega = 0^\circ$ which was able to achieve a closest approximation to the experimental result with a maximum relative error of 13%. For Specimen S2, it can be seen that all models underestimated the SIF values especially when $a'/t \leq 0.60$. However, it can be observed that acceptable correlation with test results was obtained for the crack surface $\omega = 0^\circ$ with a maximum error of about 15%.

Fig. 18 also shows that all numerical models under predicted the SIF when the a'/t is small. Note that this was not due to the density of the finite element mesh used as similar

underestimations were obtained for meshes with doubled element density. Note that the actual crack surface is curved both along the crack length and (slightly) along the crack depth. While the current model has accounted for the curvy shape in the crack length direction, the angle ω was assumed to be constant along the crack depth direction. When the crack is shallow, (i.e. small value of a'/t), the effect of the curvy crack shape in the crack depth direction might cause the underestimation of the SIF values. Finally, from the comparison of SIF values obtained from different crack surface models, it can be concluded that if the exact geometry of the crack surface angle is unknown, a value of $\omega = 0^\circ$ is recommended.

6.2 Residual fatigue life prediction

If the value of SIF at the deepest point of a surface crack is known, it is possible to estimate the residual life of a cracked joint from fracture mechanics using the Paris Law [14]:

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

In Eqn. 1, N is the number of cycles of loading applied. ΔK is the range of the SIF in one loading cycle and is equivalent to the maximum SIF when the minimum loading is set to zero. C and m are two material parameters which vary with temperature. If the values of SIF corresponding to some values of a' are computed from finite element analyses, the number of cycles required for the crack growth can be calculated by integrating Eqn. 1:

$$N_r(a') = N_{final} - N_{initial} = \int_{N_{initial}}^{N_{final}} dN = \int_{a'_{initial}}^{a'_{final}} \frac{da'}{C(\Delta K)^m} \quad (2)$$

Eqn. 2 indicates the relationship between the numbers of loading cycles, $N_r(a') = N_{final} - N_{initial}$, requires for the crack to grow from an initial size $a'_{initial}$ to the final size a'_{final} . Since in practice only a finite number of analyses could be carried out to obtain the approximated values of ΔK for some values of a' , numerical integration is required to compute Eqn. 2. Furthermore, as Eqn. 2 involves integration of the term $1/(\Delta K)^m$, it is more difficult to predict the residual life

than the SIF as *all* the errors incurred during the SIF estimation shall accumulate and contribute to the error of the residual life prediction. In addition, since $m > 1.0$, more error would be generated during the initial stage of the crack when ΔK is small.

The residual fatigue lives for Specimens S1 and S2 were estimated by Eqn. 2 using the data shown in Figs. 17 and 18. The values of material constants $C = 1.427 \times 10^{-12}$ (m/cycle)(MPa*m^{1/2})^{-3.523} and $m = 3.523$, which are corresponding to the API-5L pipes tested in ambient temperature condition [17], were adopted. The initial crack and terminal crack depths used for the residual life prediction for the two specimens are listed in Table 2.

Fig. 19 compares the residual fatigue life predictions obtained by using the proposed numerical models with crack details with different crack surface angles (measured values, $\omega \in [0^\circ, -15^\circ]$ for Specimen S1 and $\omega \in [0^\circ, 15^\circ]$ for Specimen S2) against the actual life recorded. For both specimens, as expected, the numerical models based on the measured geometry gave the most accurate predictions. For Specimen S1, all the models gave conservative residual fatigue life predictions. In addition, the finite element model with crack surface angle $\omega = 0^\circ$ gave prediction slightly more conservative than the model based on the measured crack shape. For Specimen S2, as the SIF predictions from all models (including the one based on measured geometry) are not conservative in the initiation phase of the crack propagation, the prediction of the residual life in the range $a'/t \leq 0.6$ are not conservative. However, the model with crack surface angle $\omega = 0^\circ$ could be used in practice as it produces conservative prediction when $a' > 6$ mm.

Besides plotting the predicted and actual residual life directly, a more indicative method to assess the reliability of the model is to compute the safety factors FOS_N of the residual life prediction is defined as [4]:

$$FOS_N(a') = \frac{N_{act}(a')}{N_r(a')} \quad (3)$$

In Eqn. 3, $N_{act}(a')$ is the actual number of cycles recorded for the crack to penetrate from $a'_{initial}$ to a'_{final} . Obviously, a value of $FOS_N > 1.0$ indicates that conservative fatigue life prediction. The results of FOS_N for the residual life estimation are shown in Fig. 20. For Specimen S1, Fig. 20a indicates that all numerical models are conservative. For Specimen S2, it is observed that at the initiation phase (up to $a' \approx 6$ mm) of the crack propagation, the residual life predictions for all the models are not conservative. For a more detailed investigation, the values of FOS_N for the crack surface model with $\omega = 0^\circ$ are listed in the last column of Table 2. From Fig. 20b and Table 2, it could be concluded that if the exact geometry of the crack surface angle is not available, a value of $\omega = 0^\circ$ could be employed for the residual fatigue life estimation with reasonable accuracy and reliability.

7. Conclusions and future work

In this paper, by comparing the experimental results obtained from full scale tests with the numerical modelling results, it is shown that the geometrical models and the mesh generation procedures proposed in this study could lead to consistent and reliable modelling results for partially overlapped CHS K-joints with and without crack. It is shown that the proposed finite element models could reproduce the SCF distributions along all intersection curves of the uncracked joint and the SIF values at the deepest point of the surface crack. Based on the computed SIF values, the models are able to estimate the residual fatigue life for a cracked joint with reasonable accuracy. In addition, the effects of some critical modelling parameters that may affect the accuracy of the modelling results are also studied. In particular, for the computation of the SCF values, it is found that a solid finite element model without any weld profile details tends to overestimate a lot the maximum SCF. For the prediction of SIF for a cracked joint, this study concludes that if no information regarding the crack shape is available, a model based on the zero crack shape angle could be used.

One obvious extension of the present works is to make use of them to conduct a large scale parametric study on the fatigue performance of this joint type. The results of this parametric study shall give a general picture of the fatigue performance of this joint type for the development of suitable fatigue design equations.

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(a) Actual weld size

Definitions of the Y' and Z' axes could be found in Fig. 2 of [10].

(b) Weld thickness factor k_{Tw}

Definition of the driving angle α could be found in Fig. 4 of [10].

Figure 7. Weld size along the through brace side of *Weld 3* for Specimen S2 (AWS=American Welding Society standard [12], API=American Petroleum Institute standard [15])

(a) Actual weld size

Definitions of the Y' and Z' axes could be found in Fig. 2 of [10].

(b) Weld thickness factor k_{Tw}

Definition of the driving angle α could be found in Fig. 4 of [10].

Figure 8. Finite element mesh used in the SCF/HSS analysis of Specimen S1

(a) overall deformed shape

(b) HSS distribution (under IPB loading) around *Weld 1*

Figure 9. Experimental and numerical SCF values for Specimen S1

(AX loading along *Curve 1*)

(a) SCF on the through brace side

(b) SCF on the chord side

Figure 10. Experimental and numerical SCF values for Specimen S1

(IPB loading along *Curve 1*)

(a) SCF on the through brace side

(b) SCF on the chord side

Figure 11. Finite element mesh used in the SCF/HSS analysis of Specimen S2
(a) overall deformed shape
(b) HSS distribution (under IPB loading) around *Weld 3*

Figure 12. Experimental and numerical SCF values for Specimen S2
(AX loading along *Curve 3*)
(a) SCF on the overlap brace side
(b) SCF on the through brace side

Figure 13. Experimental and numerical SCF values for Specimen S2
(IPB loading along *Curve 3*)
(a) SCF on the overlap brace side
(b) SCF on the through brace side

Figure 14. Measured values of ω
(a' = curved depth of crack, t = thickness of section, l_{cr} = length of crack, Fig. 12 of [10])
(a) Plot for Specimen S1
(b) Plot for Specimen S2

Figure 15. Measured and modelled crack shapes
(Note: a' = curved depth of crack, Fig. 12 of [10])
(a) Crack profile for Specimen S1
(b) Crack profile for Specimen S2

Figure 16. Crack branching in Specimen S1

Figure 17. Comparison of SIF at the deepest point obtained from the experimental data and the finite element model based on measured crack shape
(a) Specimen S1 (under combined loading of AX200kN+IPB45kN)
(b) Specimen S2 (under combined loading of AX200kN-IPB45kN)

Figure 18. Comparison of SIF at the deepest point predicted by using different finite element models
(a) Specimen S1 (under combined loading of AX200kN+IPB45kN)
(b) Specimen S2 (under combined loading of AX200kN-IPB45kN)

Figure 19. Prediction of residual fatigue life by using different numerical models
(a) Specimen S1
(b) Specimen S2

Figure 20. Plots of $FOS_N(a')$ against a'
(a) Specimen S1
(b) Specimen S2

Crack profile	a' (mm)	l_{cr1} (mm)	l_{cr2} (mm)	l (mm)
Specimen S1 AX 200kN+IPB45kN				
1	2.41	31.5	38.8	70.3
2	3.72	32.9	40.4	73.3
3	6.48	36.5	44.8	81.3
4	8.24	46.2	56.8	103.0
5	10.12	47.7	58.7	106.4
6	12.20	49.7	61.1	110.8
7	13.75	51.0	62.8	113.8
8	14.51	53.2	65.5	118.7
9	16.20	53.7	66.0	119.7
Specimen S2 AX 200kN-IPB45kN				
1	3.32	9.1	10.6	19.7
2	5.01	13.8	16.1	29.9
3	7.65	21.0	24.5	45.5
4	9.39	25.8	30.1	55.9
5	12.22	33.6	39.1	72.7
6	13.83	38.0	44.3	82.3
7	16.05	44.1	51.0	95.1
8	18.57	51.0	59.5	110.5

Table 1. Crack profiles used in the finite element models for SIF computation
(a' = curved crack depth, $l = l_{cr1} + l_{cr2}$ = crack length, see Fig. 13 of [10])

$a'_{initial}$ (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

(a) Parameters for Specimen S1

$a'_{initial}$ (mm)	a'_{final} (mm)	ΔK (MPa $\times$ m ^{1/2})	$N_r(a')$ (cycle)	$N_{act}(a')$ (cycle)	$FOS_N(a')$
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

(b) Parameters for Specimen S2

Table 2. Detailed results for the prediction of residual fatigue life for the model with $\omega=0^\circ$

Figure 1. Set up of specimens and test rig

Specimens	Dimensional parameters (mm, degree)						E (GPa)
	D	T	d	t	$\theta_1 = \theta_2$	p	
S1	273.0	25.0	244.5	19.1	45	21%	204.42
S2	273.0	25.0	244.5	20.0	45	21%	201.90

D =diameter of chord, T =thickness of chord, d =diameter of braces, t = thickness of braces,
 θ_1 =angle between through brace and chord, θ_2 =angle between overlap brace and chord,
 p =overlapping ratio of the joint

Figure 2. Dimensions of the specimens

Figure 3. Cyclic loadings applied to Specimens S1 and S2 during fatigue test

Figure 4. Strain gauges to measure the HSS along the intersection curves

Figure 5. Crack profile obtained from alternative current potential drop records (for Specimen S2)

(a) Actual weld size

Definitions of the Y' and Z' axes could be found in Fig. 2 of [10].

(b) Weld thickness factor k_{Tw}

Definition of the driving angle α could be found in Fig. 4 of [10].

Figure 6. Weld size along the through brace side of *Weld 1* for Specimen S1 (AWS=American Welding Society standard [12], API=American Petroleum Institute standard [15])

(a) Actual weld size

Definitions of the Y' and Z' axes could be found in Fig. 2 of [10].

(b) Weld thickness factor k_{Tw}

Definition of the driving angle α could be found in Fig. 4 of [10].

Figure 7. Weld size along the through brace side of *Weld 3* for Specimen S2 (AWS=American Welding Society standard [12], API=American Petroleum Institute standard [15])

(a) overall deformed shape

(b) HSS distribution (under IPB loading) around *Weld 1*

Figure 8. Finite element mesh used in the SCF/HSS analysis of Specimen S1

(a) SCF on the through brace side

(b) SCF on the chord side

Figure 9. Experimental and numerical SCF values for Specimen S1 (AX loading along *Curve 1*)

(a) SCF on the through brace side

(b) SCF on the chord side

Figure 10. Experimental and numerical SCF values for Specimen S1 (IPB loading along *Curve 1*)

(a) overall deformed shape

(b) HSS distribution (under IPB loading) around *Weld 3*

Figure 11. Finite element mesh used in the SCF/HSS analysis of Specimen S2

(a) SCF on the overlap brace side

(b) SCF on the through brace side

Figure 12. Experimental and numerical SCF values for Specimen S2 (AX loading along *Curve 3*)

(a) SCF on the overlap brace side

(b) SCF on the through brace side

Figure 13 . Experimental and numerical SCF values for Specimen S2 (IPB loading along *Curve 3*)

(a) Plot for Specimen S1

(b) Plot for Specimen S2

Figure 14. Measured values of ω
(a' = curved depth of crack, t = thickness of section, l_{cr} = length of crack, Fig. 12 of [10])

(a) Crack profile for Specimen S1

(b) Crack profile for Specimen S2

Figure 15. Measured and modelled crack shapes
(Note: a' = curved depth of crack, Fig. 12 of [10])

Figure 16. Crack branching in Specimen S1

(a) Specimen S1 (under combined loading of AX200kN+IPB45kN)

(b) Specimen S2 (under combined loading of AX200kN-IPB45kN)

Figure 17. Comparison of SIF at the deepest point obtained from the experimental data and the finite element model based on measured crack shape

(a) Specimen S1 (under combined loading of AX200kN+IPB45kN)

(b) Specimen S2 (under combined loading of AX200kN-IPB45kN)

Figure 18. Comparison of SIF at the deepest point predicted by using different numerical models

(a) Specimen S1

(b) Specimen S2

Figure 19. Prediction of residual fatigue life by using different numerical models

(a) Specimen S1

(b) Specimen S2

Figure 20. Plots of $FOS_N(a')$ against a'