STRESS CONCENTRATION FACTORS FOR SIMPLE TUBULAR JOINTS

Assessment of Existing and Development of New Parametric Formulae

Prepared by

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EXECUTIVE SUMMARY

This report covers the development of a new set of SCF parametric formulae for simple tubular joints, i.e., the Lloyd’s Register equations, the work being largely funded by the HSE. Additionally, the report covers an assessment of the commonly used SCF equations for simple tubular joints including the new LR equations. This latter work was carried out under the auspices of the HSE Review Panel for Fatigue Guidance (RPFG).

In the development of the LR equations, a comprehensive database of measured SCFs for full-scale steel joints and acrylic models was created. The joint acceptance criteria for this database was agreed with representatives from the Industry. From this database the new LR equations, which are given in Appendix A, were developed.

For the assessment of SCF parametric formulae for simple tubular joints the database above was refined. The finalised database, which is given in Appendix B, was used to assess the existing commonly used parametric formulae and the new LR equations. The assessment criteria which was agreed with the RPFG is given in Section 5.2 and the equations assessed are given in Appendix A.¹

¹ See footnote to Section 1.0 Introduction
NOMENCLATURE : UNSTIFFENED TUBULAR JOINT

INDEX

D - Chord diameter
T - Chord thickness
L - Chord length
d - Brace diameter
t - Brace thickness
θ - Brace to chord inclination
γ - Brace weld toe separation (K & KT Joints)
C - Chord end fixity condition
x - t/T
β - d/D
γ - D/2T
ο - 2L/D
ζ - g/D

SCF - Stress Concentration Factor - Ratio of stress to nominal brace stress
(n.b. For bending, SCF’s are relative to the extreme fibre stress)

SCFₐₐ - SCF at the chord saddle
SCFₐₜ - SCF at the chord crown
SCFₑ - Maximum SCF on the chord side
SCFₛₐ - SCF at the brace saddle
SCFₛₜ - SCF at the brace crown
SCFₛₑ - Maximum SCF on the braceside
1. INTRODUCTION

In the 1970s with the increasing development of the hot-spot stress S-N approach to nodal joint fatigue life estimation, it became clear that the determination of reliable stress concentration factors (SCFs) for tubular joints was fundamental to this concept. The first parametric SCF equations covering simple tubular joints were derived by Toprac and Beale in 1967 (1) using a limited steel joint database. The prohibitive cost of testing scaled steel models led Reber (2), Visser (3) and Kuang et al. (4,5) to use finite element (FE) analyses based on analytical models of cylindrical shells. Subsequent equations by Wordsworth and Smedley (6,7) using acrylic model specimens and by Efthymiou and Durkin (8,9) employing 3-D shell FE analyses, have made considerable advances both in the accuracy of parametric equations and in the range of joints covered.

Over this period, differences arose between the experimental procedures used to derive stress concentration factors for simple tubular joints. These differences led to inconsistencies both in the measured SCFs themselves, and also in the SCF parametric formulae based on these measured SCF values. These inconsistencies in SCF derivation are reflected in the hot-spot S-N curves used to estimate fatigue lives for simple tubular joints.

In the Lloyd’s Register (LR) group-sponsored project ‘SCFs for tubular complex joints’, parametric expressions have been derived to calculate the relief obtained by adding ring-stiffeners into a simple unstiffened tubular joint. Similar expressions also exist which give the relief obtained by grouting tubular joints. In both cases a relief factor is applied to the unreinforced joint SCF, further highlighting the need for accurate simple joint SCF equations.

In the UKOSRP II project, reported in 1987 (10), a limited programme of work investigated anomalies between the existing simple joint parametric SCF equations. However, many of the anomalies highlighted in the UKOSRP II project remain unresolved. A test programme on unstiffened tubular joints performed by Lloyd’s Register (11), has further emphasised inconsistencies between test results and the more commonly used parametric equations.

Despite considerable differences that exist between parametric SCF formulae, the current offshore installations guidance document (12), accepts the use of parametric equations to determine hot-spot stresses in tubular joints. While current guidance merely states that ‘the appropriate SCF’ should be used, it is the intention of future guidance (13) to give more specific directions on which parametric SCFs may be employed in fatigue design, and the procedures that should be considered prior to performing either experimental tests, FE analysis, or in deriving parametric equations.

In this report, 2 studies, largely funded by the HSE, are presented. In the first study a comprehensive database of steel and acrylic simple joint SCFs was created, titled the LR SCF derivation database and described in Section 3. The acceptance criteria for this database was agreed with representatives of the Industry. From this database a new set of SCF formulae for simple joints (ie the LR equations) has been derived. These new equations are given in Appendix A. In the second study the database above was refined under the auspices of the Review Panel for Fatigue Guidance. The finalised database, titled the SCF assessment database and given in Appendix B, was
used to assess existing simple joint SCF parametric formulae including the new LR equations. The equations assessed are given in Appendix A and the assessment criteria in Section 5.2.

In the SCF assessment, 5 sets of parametric equations were considered. However, in the new HSE fatigue guidance only 2 sets are considered for recommendation. Therefore, only these 2 sets, ie the new LR and Eftymiou equations, are given in Appendix A.
2. REVIEW OF EXPERIMENTAL TECHNIQUES

The HSE fatigue guidance background document (14), highlights 5 factors that influence the development of fatigue failure in welded tubular joints, and hence determine the joint fatigue life. The first of these factors describes the overall geometry of the tubular joint and the detailed geometry of its welds. For this factor, the ‘hot-spot’ is described as the region where fatigue cracking is likely to initiate due to a stress concentration. The definition of the hot-spot stress varies considerably from a very general philosophy to a detailed description of its formulation, depending upon the source quoted. During early UKOSRP studies the HSE sought a clarification in the definition of the hot-spot stress and its calculation, thus enabling the derivation of the ‘T’ S-N curve. In this project the HSE definition of hot-spot stress and design guidance has been adopted, although alternative approaches are discussed and assessed.

The definition of hot-spot stress (14), as the region where fatigue cracking is most likely to initiate due to a stress concentration, proposed linear extrapolation of the maximum principal stresses, outside the region of weld toe influence, to the weld toe. However, differences in the definition of hot-spot stress and between the type of experimental technique employed has led to considerable variation in measured stresses between nominally identical joints.

For most simple joint geometries and loadings the hot-spot will be located at either the saddle or crown. However, studies of the measured stress around the brace/chord periphery indicate that under IPB and axial load the hot-spot stress may be located at an interim position for some geometrics.

2.1 METHODS OF MODELLING TUBULAR JOINTS

In the UKOSRP I project an assessment of the performance of different modelling techniques was performed (15). This assessment concluded that differences observed between SCFs measured on steel models, acrylic models and photoelastic models or derived using a finite element analysis method, gave very similar results once differences in the local joint geometry and the experimental procedure were taken into account.

In previous studies, acrylic models and FE results have been utilised to develop parametric formulae which are verified against results from steel models. In this study the limited range of geometrics for steel joints led to the consideration of alternative methods into the database against which parametric methods would be benchmarked. This decision is discussed in detail in Section 3.

2.2 STRAIN GAUGE LOCATIONS AND STRESS SAMPLING POSITIONS

Due to the rapid increase and variation in stress around the region of the weld toe resulting from the local weld geometry, it is recommended that strain gauges should be located outside this notch region. The maximum extent of this local notch region is defined as $0.2\sqrt{rt}$ (and not less than 4 mm), where $r$ and $t$ are the brace outside radius and thickness respectively. The dependence upon $\sqrt{rt}$ has been derived from
studies of the bending stress in tubes (16), and this parameter has been empirically modified following detailed analyses of large scale tubular joint intersections in the UKOSRP and ECSC projects.

The ECSC recommendations for the extent of the notch region to $0.2\sqrt{rt}$ have been adopted by the HSE as the suggested location for the gauges nearest to the weld toe or brace/chord intersection. However, more recent work on photoelastic joints (17) suggests that for some $K$ joint configurations this notch region extends beyond $0.2\sqrt{rt}$, giving a maximum error in the order of 10% - 12%.

A second set of gauges, enabling linear extrapolation to the weld toe, are located according to the location around the brace/chord intersection:

- chord saddle = $5^\circ$arc
- chord crown = $0.4\sqrt{rtRT}$
- braceside = $0.65\sqrt{rt}$

(It should be noted that this definition does not give guidance as to the gauge location on the chordside between the saddle and the crown locations). Where non-linear extrapolation is required a third gauge set is placed equidistant from the second gauge set (eg braceside = $1.10\sqrt{rt}$).

An alternative approach developed by Gurney (18), recommended a minimum strain gauge distance of 0.4T, from the weld toe, where T is the chord thickness. Gurney’s recommendation results from FE analyses of simple fillet welded joints in plates, which indicate that the region of the notch stress is a function of the thickness of the plate upon which stresses are being measured.

In comparing the variation in SCF between the ECSC and the Gurney recommended gauge locations, Swensson et al (19) tested 3 X joints ($\beta = 0.35, 0.67$ and 1.00). These tests showed that on average the SCF derived by the Gurney method exceeds the ECSC method by 7.6%, with the least difference between the methods occurring when $\beta = 0.67$. This result corresponds with the findings of another analysis by Wardenier (20), which states that the hot-spot stress will be ‘nearly the same’ for joints with $\beta = 0.6$ irrespective of the method used, although for joints with pronounced 3-dimensional effects, eg $\beta = 1.0$, Gurney’s method describes the notch region better than the ECSC method.
2.3 MEASUREMENT OF STRESSES AND STRAINS

Strains are measured on physical models using either strain gauge rosettes or via sets of single strain gauges placed perpendicular and parallel to the joint intersection or weld toe. The methodology for determining maximum principal stresses from measured strains is shown below:

\[
\sigma_{\text{max}}/\sigma_{\text{min}} = \frac{E}{2} \left[ \frac{(\varepsilon_a + \varepsilon_c)}{1-\nu} \pm \frac{\sqrt{2}}{1+\nu} \sqrt{(\varepsilon_b - \varepsilon_a)^2 + (\varepsilon_b - \varepsilon_c)^2} \right]
\]

Where \( E \) = Young’s modules of the material
\( \nu \) = Poisson’s ratio of the material

For angle of maximum principal stress \((\alpha)\)

\[
\tan 2\alpha = \frac{\varepsilon_b - \varepsilon_a - \varepsilon_c}{\varepsilon_c - \varepsilon_a}
\]

It has been suggested by Lalaini (21), that for vertical braces the angle of the maximum principal stress \((\Psi)\) tends to lie almost perpendicular to the joint intersection (ie \( \Psi = 0^\circ \)). Consequently the difference between the SCF obtained from principal stresses and from strain concentration factors (SNCFs) is minimised. As the angle of the maximum principal stress increases, possibly due to the brace angle decreasing, the difference between SCF and SNCF will tend to increase).

Three ways of utilising the strain gauge results to calculate the SCF or SNCF were assessed by Lloyd’s Register in this study:

a) Extrapolation of the **maximum principal stresses**.

b) Extrapolation of the **strains perpendicular** to the weld toe.

c) Conversion of the strain concentration factor calculated in method (b) to a biaxial stress using:

\[
SCF = \frac{SNCF + \nu \cdot SNCF_90}{(1 - \nu^2)}
\]
where $v$ is the Poisson’s ratio of the model material ($v = 0.30$ steel, $v = 0.36$ acrylic) and $\text{SNCF}_{90}$ is the strain concentration factor from the nearest parallel gauge to the weld toe or joint intersection.

In this study, it was found that for 90° joints, the maximum principal stress (method (a)) was approximately 20% - 25% larger than the perpendicular strain (method (b)) for acrylic models, while for steel models the difference was nearer to 15%. This can be accounted for by the different Poisson’s ratio for the 2 materials. Furthermore, for both steel and acrylic 90° joint specimens, it was confirmed that the angle of the maximum principal stress varied by less than 10% from the perpendicular to the joint intersection.

These results correspond well with the UEG design guide (22) which recommends an overall factor of 1.20, while examination of a K joint by Lalani (21) suggest a lower factor of 1.16.

For both steel and acrylic joints with inclined braces, the angle of the maximum principal stress increased as the brace angle decreased. However, far more significant differences occur between the maximum principal stress and the perpendicular strain on steel joints (30% - 50%) than on similar acrylic model specimens (23% - 30%) despite the aforementioned difference in the Poisson’s ratio. This inconsistency between the limited number of steel and acrylic joint specimens reviewed requires investigation in future testing programmes.

For all joint configurations, the SCF derived from maximum principal stress (method (a)) is consistently 2% - 3% larger than the SCF derived from biaxial stress (method (c)).

### 2.4 EXTRAPOLATION PROCEDURE

The HSE recommendations regarding extrapolation technique requires linear extrapolation to be adopted for 90°T and X joints ($\beta<1$), while for inclined Y and K joints it is stated that in some cases there may be a non-linear geometric stress distribution. However, the hot-spot stress S-N approach for simple tubular joints is currently based on linear extrapolation, irrespective of the joint configuration.

The HSE does not give a method for extrapolating stresses, however, the extrapolation procedures adopted by Lloyd’s Register in conjunction with acrylic model testing are given in Appendix C.

Lloyd’s Register have reviewed 67 simple acrylic T/Y, X and K joints that employed both linear and non-linear extrapolation of maximum principal stresses to the brace/chord intersection (23). Linear extrapolation was performed through the 2 rosettes nearest to the brace/chord junction, while non-linear extrapolation, employing a quadratic fit or Lagrange polynomial, utilised these 2 rosettes and a third rosette positioned on equal distance from the second rosette.

It was concluded that non-linear extrapolation exceeds linear extrapolation on the chordside for all loadcases by less than 5% irrespective of brace angle. On the braceside, particularly at the brace crown under IPB, larger differences of around 10% can be found, although the degree of brace inclination does not appear to be a significant factor, see Table 2.1. However, it should be noted that to give a realistic
assessment of the differences between linear and non-linear extrapolation, joints where $\beta = 1$ or where the measured SCF was less than 1.5 were excluded. For $\beta = 1$ joints (particularly $\beta = 1$ X joints) where the stresses tend to be relatively small, SCFs using non-linear extrapolation could be twice those using linear extrapolation (e.g., SCF = 0.41 linear, and SCF = 0.76 non-linear).

### Table 2.1
Increase in SCF using non-linear extrapolation

<table>
<thead>
<tr>
<th>Joint config</th>
<th>No of joints</th>
<th>Chord Axial Load</th>
<th>OPB</th>
<th>IPB</th>
</tr>
</thead>
<tbody>
<tr>
<td>T</td>
<td>20</td>
<td>4%</td>
<td>0%</td>
<td>6%</td>
</tr>
<tr>
<td>Y</td>
<td>6</td>
<td>4%</td>
<td>-</td>
<td>-1%</td>
</tr>
<tr>
<td>X</td>
<td>9</td>
<td>3%</td>
<td>5%</td>
<td>6%</td>
</tr>
<tr>
<td>K</td>
<td>22</td>
<td>4%</td>
<td>-1%</td>
<td>1%</td>
</tr>
</tbody>
</table>

### 2.5 THE INFLUENCE OF A WELD FILLET

Acrylic model tests and some FE analyses do not include a weld fillet on the tubular joint intersection, and therefore require some modification to their measured hot-spot stress if they are to be compared to steel joint test results. The addition of a weld fillet to a tubular connection results in a reduced SCF which is measured at the weld toe rather than at the joint intersection.

It is important to note that the extrapolation of stresses on a model with no weld fillet should not be foreshortened to where the weld toe would be in an attempt to reflect the influence of the weld.

#### 2.5.1 The inclusion of a weld fillet

Employing acrylic model T and 90° X joints, Smedley (24) produced a weld fillet correction factor based on the chordside weld fillet leg length.

$$SCF_{\text{Weld toe}} = \frac{SCF_{\text{No weld}}}{3\sqrt{(1 + \frac{x}{T})}}$$

(Where $x$ is the weld fillet leg length on the chordside)

While being specifically designed for the chordside stress on 90° joints, Smedley also considered this expression to be acceptable for 90° joints on the braceside. However, for inclined braces it was considered that this factor was not applicable.

Alternatively, Marshall (25) proposed a design equation, based on FE analysis of K joints.

$$SCF_{\text{Brace weld toe}} = 1.0 + [SCF_{\text{Brace mid-surface}} - 1.0] \times \exp \left(-\left(\frac{0.5T_x}{d_T}\right)\right)$$

This equation, applied to the braceside mid-section stress, uses an exponential decay function to approximate the SCF at the braceside weld toe, and is generally used in conjunction with the Kuang parametric equations (5).
A more recent approach (26), suggested that for a specimen with no weld fillet, the extrapolation to the brace/chord intersection should be foreshortened by half the standard fillet leg length to simulate the inclusion of a weld fillet, see Figure 2.2. Further work by Lloyd’s Register using this approach on joints with $\beta<1$, showed a consistent stress reduction factor of 0.95 on the chordside and 0.88 on the braceside, when a weld fillet leg length = $t/2$ on the chordside and $= t$ on the braceside was described in accordance with API recommendations for controlled weld profiles (27). It should be noted that for many of the steel test specimens, particularly those of small scale, the weld leg length on the braceside exceeded the brace thickness ($t$).
Figure 2.2
Reduction in SCF due to the inclusion of a weld fillet

An investigation into the effect upon the SCF of a weld fillet was undertaken by Efthymiou (28), using a 3-D FE analysis on one T joint configuration with and without a weld fillet, see Table 2.2.
Table 2.2
FE analysis of a joint with and without a weld fillet (linear extrapolation)

<table>
<thead>
<tr>
<th>Loading</th>
<th>Chordside</th>
<th></th>
<th>Braceside</th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>No weld</td>
<td>Inc weld</td>
<td>No weld &amp; Inc weld</td>
<td>No weld</td>
</tr>
<tr>
<td>Axial</td>
<td>24.52</td>
<td>23.50</td>
<td>0.958</td>
<td>14.95</td>
</tr>
<tr>
<td>OPB</td>
<td>20.29</td>
<td>19.54</td>
<td>0.963</td>
<td>12.40</td>
</tr>
<tr>
<td>IPB</td>
<td>6.63</td>
<td>6.18</td>
<td>0.932</td>
<td>5.09</td>
</tr>
<tr>
<td>Mean reduction factor</td>
<td>0.951</td>
<td></td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

(Joint parameters: $\beta = 0.5$, $\tau = 1.0$, $\gamma = 28.6$ and $a = 5.0$)

It can be seen in Table 2.3, that for the joint geometry modelled by Efthymiou, the Wordsworth weld reduction factor gives the closest agreement to the measured weld fillet reduction factor.

Table 2.3
Reduction in SCF due to the inclusion of a weld fillet

<table>
<thead>
<tr>
<th></th>
<th>Measured factor</th>
<th>Smedley factor</th>
<th>Marshall factor</th>
<th>Wordsworth factor</th>
</tr>
</thead>
<tbody>
<tr>
<td>Chordside</td>
<td>0.95</td>
<td>0.87</td>
<td>1.00</td>
<td>0.95</td>
</tr>
<tr>
<td>Braceside</td>
<td>0.86</td>
<td>0.87</td>
<td>0.69</td>
<td>0.88</td>
</tr>
</tbody>
</table>

2.5.2 Weld cut-back at the saddle location on $\beta = 1$ joints

Following a series of steel joint tests on $\beta = 1$ T joints under OPB, Wylde (29) described the variation in measured SCFs between tubular members of different diameter. On the chordside, the SCFs on 168 mm diameter joints were more than twice the SCF values on 457 mm diameter joints. Wylde felt that the difference between the 2 types of joint was most likely due to the differences in the weld profiles rather than the joint size. It was concluded that caution should be applied to $\beta = 1$ joints since no parametric equation takes into account the weld profile.

It was noted by Wordsworth (26) and confirmed in this project, that the largest differences between measured SCFs and predicted SCFs occur when the brace diameter approaches the chord diameter. Some of these inconsistencies in measured SCF values may be attributed to the varying degrees of weld cut-back used in the test specimens, see Figure 2.3. At the saddle location, the SCF decreases rapidly as $\beta$ approaches 1. For example, using the Wordsworth/Smedley equation (6) on a T joint, the SCF increases by 38% under axial load and by 50% under out-of-plane bending by altering $\beta$ from $\beta = 1.0$ to $\beta = 0.95$.

For $\beta = 1$ joints with ‘extreme’ weld cut-back, the separation between the weld toes at the saddle locations may be considerably less than the chord/brace diameter. It was suggested by Wordsworth that for joints with equal chord and
brace diameters, $\beta$ should be defined as the weld toe separation relative to the chord diameter (i.e., $d'/D$). However, it was noted that the weld toe separation $d'$ is neither easily calculated nor measured, see Figure 2.4.

Following a detailed analysis of the parameter $\beta' = d'/D$, Lloyd’s Register found that $\beta'$ may be reasonably estimated by:

$$\beta' = 1 - \left( \frac{t}{t_x} \sin 0.65 \left( \Psi^o \right) \right)$$

Where $\Psi^o$ is the degree of weld cut-back

(If $\Psi$ is not defined, a default value of $\Psi = 20^\circ$ is suggested).

This expression has been included in the new Lloyd’s Register parametric equations for simple tubular joints. By employing this expression for $\beta = 1$ joints, SCFs may be derived to a greater accuracy for these complex joint configurations, and consequently more accurate fatigue lives may be estimated.

### 2.5.3 Weld fillet profile

The profile of the weld fillet was examined in an FE analysis of X joints by Lieurade (30). This study compared concave, straight and convex weld profiles and concluded that outside the notch region the weld profile was not a highly significant
factor, although a concave weld will always give the lowest stress. Since most joints are generally modelled to a concave weld profile there will be negligible influence on the hot-spot stress due to marginal differences in weld profile.

2.6 CHORD LENGTH AND CHORD END EFFECTS

Most test specimens are constrained by their physical size to an $\alpha = 2L/D$ value in the range $\alpha = 5$ to $\alpha = 15$, although Wordsworth/Smedley (6) performed tests on acrylic models with $\alpha$ values up to $\alpha = 40$. In practice over 60% of nodes have a values in excess of $\alpha = 20$, see Figure 2.5 and 35% of nodes exceed the $(\alpha = 40)$ upper bound for $\alpha$ recommended by most of the parametric SCF equations.

![Bar chart showing the distribution of $\alpha$ values for 3760 joints from 4 North Sea Platforms]

In the literature study performed at the beginning of this project, it was frequently found that there was very little if any information given about the chord length, chord end fixity or the test rig structure. Therefore all joints in this project were assumed to be pinned at the chord ends when assessed against the parametric equations. Furthermore, confusion often arises over the somewhat ambiguous definition of the chord length and $\alpha$ parameters in both test specimens and in structural nodes. The problem is further complicated in the test rig by variations in the clamping of the chord (ie pinning or fixing the chord ends), and the inclusion of rigid diaphragms which act in a similar manner to ring-stiffeners.
2.6.1 Supported chord effects at the crown under axial load

The SCF at the crown location under axial load in T/Y joints is composed of a local shell deformation effect and an overall chord in-plane bending effect that is linear in \( a \). This fact is not recognised by the Kuang (5) and HCD (31) equations which will eventually lead to underprediction in the crown SCFs as \( a \) increases. The chord in-plane bending term (\( B_0 \)) has been derived from the brace axial force applied to a simply supported centrally located brace where:

\[
B_0 = \frac{C \tau (\beta - \beta(2\tau)) (a/2 - \beta/\sin \theta) \sin \theta}{(1 - 3/(2\tau Y))} \approx C \tau \beta (a/2) \sin \theta
\]

While \( B_0 \) gives a good estimate of the bending stresses in a test specimen, the stresses in the chord wall in an offshore structure are not simply related to the brace axial force. Consequently, it is almost certain that a poor SCF result will be obtained.

In this assessment of the T/Y joint SCF equations, it was found that there were considerably more SCF underpredictions for the crown under axial load than for other loadcases/locations. An analysis of these results concluded that the predicted SCFs were underestimating the measured SCFs due to an incorrect interpretation of \( a \) in the chord in-plane bending term. In some cases the quoted value of \( a \) is based on the physical chord length, whereas for the chord in-plane bending at the chord crown \( a \) should be based on the separation of the pinned supports. For some steel joints with relatively short chord lengths the quoted value of \( a \) increased by around 30% once the pinned supports were also considered.

Since the overall structural FE analysis readily gives the chord in-plane bending and axial stresses, then a simpler and better analysis would result from utilising these known FE values rather than estimating these effects from simply supported joint approximations. In the Lloyd’s Register fatigue analysis using the PALS software package, it was found that this approach could lead to significant differences in the overall hot-spot stress for some joint configurations when compared to the approximation described above.

2.6.2 Short chord effects at the saddle

Unlike the crown where the chord length influences the degree of bending in the chord, any chord length effects at the saddle will be due to the restriction on chord ovalisation imposed by the chord end diaphragms. Therefore at the saddle, the quoted \( a \) value should be based on the separation of the chord end diaphragms (ie the physical length of the chord specimen).

Wordsworth/Smedley tested joints with \( a \) varied from \( a = 13.5 \) to \( a = 40.0 \), and concluded that chord length was not a significant factor at the saddle. However Efthymiou and Durkin (8) found that joints with short chord lengths (\( a < 12 \)) exhibit a significant reduction in the SCF at the saddle, due to the restrictions on chord ovalisation caused by the locality of the chord end supports.

Chord end diaphragms used to support the test specimen, act in a similar manner to ring-stiffeners and restrict the degree of chord ovalisation. Consequently, Efthymiou and Durkin proposed correction factors to be able at the saddle location on joints with short chord lengths. These correction factors were found to depend not only on
$a$, but also on $\beta$ and $\gamma$. So that for a T joint under axial load with a relatively thin chord wall and $a = 5$, the short chord correction factor can halve the saddle SCF compared to a similar node with $a = 10$.

Most steel joint test specimens are constrained to having $a$ values in the range $a = 5$ to $a = 10$, with the larger diameter joints tending to have the smaller $a$ values. Therefore, nearly all the SCFs derived from steel joint test results are influenced by the locality of the end restraint, and as a consequence the SCFs measured on test specimens may be significantly less than those observed on more realistic joints. It should be emphasised that this is a chord end ovalisation effect rather than a chord support effect. Therefore, for most X joints where the joint is supported by the brace ends and no chord end diaphragms are fitted, chord length effects are far less pronounced.

As steel joint test specimens are used as a benchmark for testing the validity of parametric equations, it is important that end effects are taken into account prior to any assessment. In future, it is recommended that consideration should be given to short chord effects before any physical model or FE analyses are performed, whether for static strength, SCF or fatigue endurance.

3. DATABASE

This section describes the Lloyd’s Register SCF derivation database. This database of steel and acrylic joint SCFs was created for the development of a new set of parametric equations for simple tubular joints. The work was largely funded by the HSE.

At the outset of the project lengthy discussion took place with both the HSE and with many representatives from the oil industry regarding the composition of this database. It was felt that steel data alone was insufficient to enable a new set of parametric equations for simple joints to be developed, particularly for X, K and KT joints. It was considered that acrylic models gave a good representation of the stress distribution observed in equivalent steel joints, and that acrylic joints should be included in the database provided allowance was made for differences in experimental techniques. This conclusion was supported by assessments of experimental techniques performed in the UKOSRP I project (15).

With regard to FE results, it was felt that published data was of variable quality and often the analysis methodology was insufficiently documented. Therefore, it was decided to exclude SCFs derived by FE analysis from the database, while accepting that FE analysis, particularly using 3-D shell elements, generally gives good correlation to similar steel joint configurations. The database of photoelastic model tests was considered too small to be validated independently.

3.1 CRITERIA FOR THE ACCEPTANCE OF SCF DATA

The initial objective of this project was to gather measured SCF data relating to simple tubular joints. To meet this objective, a literature search was undertaken to locate experimental data relevant to this project. In association with SCF results from work performed in-house, this literature search has provided a substantial database for this project.
The following data items are included:

a) Source of the SCF data - title of paper, authors, where the data has been located, the date of publication and any confidentiality restrictions.

b) Experimental details - specimen material, extrapolation type, location of the nearest gauge, stress/strain measurement, weld fillet inclusion and chord end fixity.

c) Number and configuration of the joints tested.

d) Geometric and parametric details of the joint - \( (D, T, L, d, t, \theta, \tau, \beta, \gamma, \alpha) \).

e) Applied loadcases - (axial, OPB, IPB - single, balanced, unbalanced).

f) The measured SCFs and their location on the joint - (chordside or braceside - saddle, crown or an interim location).

g) Repetitions - where several configurations were tested with the same joint parameters, the results of each test were included. However, the danger of bias in the analysis was noted.

h) Several saddle results - where 2 or 4 individual SCFs are quoted for one joint, the maximum SCF result was taken.

i) Tension/compression - where a joint is axially loaded in both tension and compression, the average of the 2 results was taken.

Verifying the accuracy of the data involved locating the source paper or experimental reports where possible and comparing the given results to a second publication. This exercise was time consuming and not always feasible, however the necessity of this cross-checking procedure was proved with several inconsistencies in SCF results occurring between published papers.

(i) Single-plane, non-overlapped, unstiffened and ungrouted tubular joints, with a chord diameter \( \geq 150 \text{ mm} \) and the strain gauge nearest to the brace/chord intersection being outside the notch region, were recorded on the database.

The chord length quoted in most papers was the physical length of the test specimen, and this was the value recorded in the database. However, it would have been desirable to record 2 'chord length' values if these had been quoted. The separation between the chord end diaphragms is required to derive ovalisation effects, and the separation between the chord end supports is required to calculate the degree of chord bending in the test specimen.

Only joints with parameters typical of those noted in practice were included, and the following subset of data was felt to be typical of offshore platforms: \( \tau \leq 1.05, \gamma \leq 40, \text{SCFs} \geq 1.5, \beta \leq 1.0 \) (only \( \beta = 1 \) joints at the saddle where the degree of weld cut-back is recorded).
(ii) The initial analysis of the SCF data involves the standardisation of the measured SCFs to conform with the HSE recommended method, this has been achieved by the use of a number of correction factors covering differences in the experimental derivation of the hot-spot stress, see section 3.2.

3.2 FACTORS APPLIED TO THE SCF DATABASE

- Strain gauge locations: the nearest strain gauge to the weld toe or to the brace/chord intersection must be in excess of a distance of 0.18\(\sqrt{\text{rt}}\) from the weld toe or the brace/chord intersection. This is based on a 10% reduction in the recommended value of 0.2\(\sqrt{\text{rt}}\), since some papers appear to round strain gauge location to the nearest mm. This small allowance did not result in a significant increase in the extrapolated SCF value.

- Maximum principal stress or biaxial stress derived from strain concentration factors: it is considered that no factor is required to differentiate between SCFs derived from either biaxial stress or maximum principal stress.

- Stress and strain measurement: the recommended factors for converting perpendicular strain (SNCFs) to maximum principal stress (SCFs) are:

\[
\text{SCF} = \text{SNCF} \times 1.15 \text{ for T joints (steel joints).} \\
\text{SCF} = \text{SNCF} \times 1.23 \text{ for T joints (acrylic joints).}
\]

For inclined braces larger differences between SNCFs and SCFs would be expected, however there is insufficient data on strain concentration factors from inclined braces to derive an estimate of this difference. These factors were applied to all braces despite these limitations.

- Extrapolation procedure: the recommended factor for converting non-linear SCFs to linear SCFs is:

\[
\text{Linear} = \text{non-linear} \times 0.95 \text{ (including } \beta = 1 \text{ joints).}
\]

This factor is less than the 10% difference proposed by UEG (22), but would appear to give a better estimate of the difference between linear and non-linear stress since this value is based on a database of 57 joints analysed in the LR project (23).

- The absence of a weld fillet: weld fillet correction factors of 0.95 on the chordside and of 0.86 on the braceside have been adopted, these values are based on the only measured weld fillet comparison available (ie the KSEPL joint) (28).

- Weld cut-back at the saddle location on \(\beta = 1\) joints: following a detailed analysis of the parameter \(\beta = d'D\), Lloyd’s Register found that \(\beta'\) may be reasonably estimated by:

\[
\beta' = 1 - \left(\frac{\chi}{\gamma} \sin^{0.65} (\Psi^o)\right) \text{ Where } \Psi^o \text{ is the degree of weld cut-back} \\
\text{(If } \Psi \text{ is not defined, a default value of } \Psi = 20^\circ \text{ is suggested).}
\]
This expression has been included in the new Lloyd’s Register parametric equations for simple tubular joints, listed in Appendix A.

It should be noted that many steel joints in particular, have \( a \) values based on the separation of pinned end supports rather than chord end diaphragm separation. This gives a more accurate assessment of the SCF at the crown which is dependent upon the level of bending in the chord, while the reduction in SCF due to the restriction on chord ovalisation noted at the saddle on joints with short chord lengths tended to be underestimated.

Chord length effects due to the restriction of ovalisation caused by the diaphragms at the chord ends leads to considerable reduction in SCFs at the saddle for short chord lengths. This problem is described by Efthymiou (8) who published short chord correction factors to be used in accordance with the Efthymiou SCF equations when \( a (=2L/D) <12 \). A substantial number of steel T joints were tested with \( a \) values in the range \( a = 4.3 \) to \( 12.0 \). To eliminate these joints from the database would have had a substantial effect on the number of steel joints that could be considered.

### 4. SIMPLE JOINT SCF EQUATIONS

The parametric equation gives the designer the most convenient way of estimating the hot-spot stress in simple tubular joints, however at the present time no equations are recommended by the HSE. Even between the more commonly used parametric equations, there are significant differences in the way the hot-spot stress is defined, calculated, and in their recommended ranges of applicability. It should be noted that some of the comments made here are based on the assessment covered in Section 5.0.

#### 4.1 KUANG EQUATIONS (1975 AND 1977)

The Kuang equations (5), cover T/Y, K and KT joint configurations and utilise a modified thin-shell finite element program specifically designed to analyse tubular connections. The tubular connections were modelled without a weld fillet, and stresses were measured at the mid-section of the member wall. Therefore the stresses calculated using the Kuang finite element models are considerably different from the HSE definition of hot-spot stress. The Kuang equations are based on a mean fit to the database of FE joints examined, and do not indicate the location of the hot-spot around the periphery, but are merely expressed as chordside or braceside.

With regard to the Kuang equations, the following points were noted:

(i) Of the equations reviewed, the Kuang equations have the most restricted validity range, and consequently cover the fewest joints in the database. The Kuang equations were not designed to cover joints with \( f/b > 0.8 \) and do not cover X joints or the unbalanced OPB loadcase for K and KT joint configurations.

(ii) For T/Y joints under axial load, no account is given to the beam bending effect, and consequently underpredictions at the crown for high \( a \) values may be anticipated.
(iii) No account is given to chord length effects at the saddle, due to the chord end restraints. Therefore, given that the database of FE joints utilised by Kuang had generally short chord lengths, there is a possibility of underestimation of SCFs for more realistic chord length \((a)\) values.

(iv) The performance of the Kuang equations for T/Y joints for \(\beta\) values above 0.5 is generally poor, although there can be considerable variation in the degree of underprediction depending upon the loadcase considered. For example, the Kuang equation gives a very poor fit to chordside SCFs under OPB underpredicting 70% of joints in this database, yet gives a reasonably good fit to the braceside SCFs under OPB.

(v) For K joints, the Kuang equations are generally conservative for all values of \(\beta\). Non-symmetric K joint configurations exhibit the largest difference between the measured SCFs and the predicted SCFs, since the Kuang K joint equations were specifically designed for joints with symmetric braces.

(vi) For KT joints under balanced axial load, the Kuang equations show good agreement with the measured SCF values on the chordside. However, on the braceside, the Kuang KT joint SCF equations differs considerably from the corresponding equation for a K joint, with the predicted SCFs for the KT joints in this database up to 4 times larger than the measured SCF values.

The Kuang equations are still widely used in the fatigue design of offshore tubular joints.

4.2 WORDSWORTH/SMEDLEY EQUATIONS (1978 AND 1981)

The Wordsworth/Smedley (W/S) equations were derived using acrylic model test results on tubular joints modelled without a weld fillet. The equations covering T/Y and X joint configurations were published by Wordsworth/Smedley in 1978 (6), and the K and KT joint configurations were covered by Wordsworth in 1981 (7). Generally, the HSE recommendations for the derivation of the hot-spot stress were followed, using maximum principal stresses from outside the \((0.2 \sqrt{r})\) ‘notch’ zone. However, some areas of uncertainty do exist. The first surrounds the extrapolation technique adopted, which appears to be generally linear except where the stress distribution was found to be particularly non-linear and therefore open to error. The second concerns the number of sets of strain gauges adopted around the brace/chord intersection. The Wordsworth parametric equations specifically cover the saddle and crown, but it is unclear whether interim sets of gauges were adopted, particularly under IPB where for some configurations the hot-spot stress occurs between the saddle and crown.

For the W/S and Wordsworth equations, the following points were noted:

(i) On the braceside, for all loadcases and measuring locations, the W/S and Wordsworth equations estimate the SCF using a simple factor applied to the chordside SCF. Consequently, the predicted SCFs on the braceside tend to be rather conservative relative to the measured braceside
SCFs. However, it should be noted that the braceside SCF on a simple tubular joint rarely exceeds the chordside SCF. The Wordsworth equations generally give a good estimate of the measured chordside SCFs.

(ii) For joint configurations with equal chord and brace diameters (ie $\beta = 1$ joints), a $\beta$ value of $\beta = 0.98$ was taken at the saddle location as recommended by Wordsworth to simulate the weld cut-back found at the saddle in typical steel $\beta = 1$ joints.

(iii) The Wordsworth equations for K and KT joints utilise carry-over functions applied to the T joint expressions. Therefore the influence of adding further braces to a simple T joint can clearly be determined.

(iv) Under axial loading and OPB at the saddle location, the Wordsworth equations tend to underpredict measured SCFs on joints with $\beta = 0.8$ and high $\gamma$, and joints with $\beta = 1.0$ where there is a significant degree of weld cut-back.

(v) The W/S and Wordsworth equations are only valid for chord radius to thickness ratios $R/T \geq 12$. A significant number of tubular joints are designed with relatively thick chords, to avoid the use of ring-stiffeners. Consequently, $R/T$ ratios in the range $8 \leq R/T < 12$ are not uncommon in practice, and by using the W/S and Wordsworth equations a conservative assumption of $R/T = 12$ must be made in determining the SCF.

4.3 UEG EQUATIONS (1985)

The UEG equations proposed in 1985 (22), are based on the W/S and Wordsworth equations with a modification factor applied to configurations with high $\beta$ ($\beta > 0.6$) or high $\gamma$ ($\gamma > 20$) values.

The following points may be made with regard to the UEG equations:

(i) All the comments for the W/S equations hold true for the UEG equations except for the weld-cut back simulation at $\beta = 1$, which is accounted for in the $Q'_\beta$ term.

(ii) The factors $\sqrt{Q'_\beta}$ and $Q'_\gamma$ are both applied under axial load and OPB while only $\sqrt{Q'_\gamma}$ is applied under IPB where:

$$Q'_\beta = 1.0 \quad \text{for} \quad \beta \leq 0.6$$
$$Q'_\beta = 0.3/\beta(1 - 0.833/\beta) \quad \text{for} \quad \beta > 0.6$$

$$Q'_\gamma = 1.0 \quad \text{for} \quad \gamma < 20$$
$$Q'_\gamma = 480/\gamma(40 - 0.833/\gamma) \quad \text{for} \quad \gamma \geq 20$$

These modification factors based on comparisons of predicted and measured results from both static and fatigue studies, apply to all joint configurations and are designed to give a characteristic set of equations (ie underpredicting 5% of the measured data).
4.4 EFTHYMIOU/DURKIN EQUATIONS (1985 AND 1988)

In 1985, Efthymiou and Durkin (8) published a series of parametric equations covering T/Y and gap/overlap K joints. Over 150 configurations were analysed via the PMBSHELL finite element program using 3-dimensional shell elements, and the results were checked against the SATE finite element program for one T joint and 2 K joint configurations. The hot-spot SCFs were based on maximum principal stresses linearly extrapolated to the modelled weld toe, in accordance with the HSE recommendations, with some consideration being given to boundary conditions (ie short chords and chord end fixity).

In 1988, Efthymiou (9) published a comprehensive set of simple joint parametric equations covering T/Y, X, K and KT simple joint configurations. These equations were designed using influence functions to describe K, KT and multi-planar joints in terms of simple T braces with carry-over effects from the additional loaded braces.

With respect to the Efthymiou/Durkin equations, the following points may be noted:

(i) It has been shown by Efthymiou that the saddle SCF is reduced in joints with short chord lengths, due to the restriction in chord ovalisation caused by either the presence of chord end diaphragms or by the rigidity of the chord end fixing onto the test rig. Therefore, the measured saddle SCFs on joints with short chords may be less than for the equivalent joint with a more realistic chord length. Factors have been included in the Efthymiou parametric equations to cover short chords.

(ii) The T/Y joint equation for the saddle under axial load includes a short chord correction factor for either fixed or pinned ends. The short chord effect at the saddle is due to the presence of chord end diaphragms, therefore, it is unclear why the chord end fixity should be a factor. The equation for X joints at the brace crown under axial load does not equal the corresponding T/Y joint equation excluding chord bending terms, as would be expected.

(iii) The Efthymiou equations give a comprehensive coverage of all the parametric variations and are designed to be mean fit equations. Due to the greater correlation with steel models by the Efthymiou FE models, and the fewer conservative assumptions made, these equations tend to be nearest to a mean fit and consequently more underpredictions are frequently observed.

(iv) Under unbalanced OPB, the Efthymiou equations give a good fit to symmetric K joints or the outer braces in KT joints, but consistently appear to underestimate the SCF in the branch with $\theta_{\text{max}}$ in non-symmetric K joints.

4.5 HELLIER, CONNOLLY AND DOVER EQUATIONS (1990)

The Hellier, Connolly and Dover (HCD) equations (31) were published in 1990 and were primarily developed to improve fracture mechanics estimates of remaining life for a joint rather than for tubular joint design. Consequently, the overall programme included not only hot-spot stress estimates, but modelling of the stress distribution around the brace/chord intersection and the proportions of bending to axial stress through the member thickness. The SCF equations themselves cover T/Y joint configurations alone, and have a range of applicability for the $\beta$ parameter limited to...
\( \beta \leq 0.8 \). Therefore, these expressions are currently limited in their application, however, further work is intended for \( \beta = 1 \) joints and for X and K joint configurations.

A thin-shell finite element method was developed using the PAFEC package, and in a similar manner to the work by Kuang weld fillets were not modelled. It has been noted in previous numerical modelling of tubular joints using the PAFEC semi-loof FE package (32) that poor results were obtained for large brace relative to chord diameters, and hence an upper recommended limit of \( \beta = 0.8 \) was proposed. In this study for \( \beta \) values near to \( \beta = 0.8 \), the HCD and Kuang equations often gave similar predictions, which differed from the other equations reviewed. Therefore, further correlation studies between the PAFEC FE package and physical specimens would be beneficial for large \( \beta \) ratios.

In a similar manner to the Kuang equations, the HCD equations were initially derived using the simple form:

\[
SCF = a_1 a a_2 \beta a_3 \gamma a_4 \tau a_5 \theta a_6
\]

where variables \( a_1 \) to \( a_6 \) are determined using a regression analysis method. The expressions were then refined by the addition of further terms.

With regard to the HCD equations, the following points were noted:

(i) For T/Y joints, at the chord crown under axial load, the linear overall beam bending effect has not been accounted for in the expressions. The chord length \( a \) effect was deemed to have little effect for \( a \) values greater than 13.1, consequently this upper bound test parameter was removed, so that these expressions are applicable for all \( a \) values exceeding \( a = 6.21 \). Furthermore, the majority of specimens modelled were influenced by chord end constraints, but again no account of this effect was taken. The failure to consider chord length effects must have an influence on the estimation of SCFs for joints with more realistic chord lengths.

(ii) With the exception of the \( \beta \) range, these expressions give comprehensive coverage of realistic geometries observed on offshore structures, and it is particularly worth noting that the chord radius to thickness ratio \( (\gamma) \) was studied to as low as \( \gamma = 7.6 \).

(iii) The derived expressions estimate the SCF at both the saddle and crown locations and also the maximum SCF around the brace/chord intersection. In addition, these expressions give the angle around the brace/chord intersection of the maximum SCF. These expressions give a comprehensive description of the stress distribution around the periphery, for all loadcases, on both the chordside and braceside. However, some anomalies are associated with these expressions. The position of the hot-spot stress may be identified as being at the saddle or crown, but the hot-spot SCF equation (eg chordside) may not give the same result as the saddle/crown equations. Cases were observed, where the hot-spot SCF (at the saddle) exceeds the saddle equation SCF by up to 20\%, and in other cases the saddle equation SCF was larger than the hot-spot (saddle) SCF.
For symmetric 90° T joints the 2 crown positions would yield the same SCF, however the 0° and 180° expressions for IPB do not give the same SCF.

4.6 LLOYD’S REGISTER EQUATIONS (1991)

The Lloyd’s Register (LR) equations were developed as part of the “SCFs for simple tubular joints” project which was largely funded by the HSE, in 1991. An initial review of the performance of existing SCF equations with regard to the LR SCF database highlighted a number of anomalies in the goodness of fit of all these expressions. Therefore the HSE suggested that a consistent set of SCF equations should be developed.

The LR equations (33) were developed as mean fit equations to the LR SCF derivation database, described in Section 3, by minimising the percentage difference between the measured SCF values and the estimated SCF values. The objective of these equations was to employ influence functions wherever possible, so that the expression for one brace on a K joint would be that for an equivalent Y joint with factors to describe the stiffening effect of the additional brace and loading given the magnitude of the load applied to this additional brace. Furthermore, consideration was to be given to \( \beta = 1 \) joints where particular problems had been identified due to the degree of cut-back of the weld at the saddle location. Short chord effects were also considered to be of particular significance, however it became clear that for the data available to LR derivation of short chord correction factors could not be achieved. Therefore, the Efthymiou short chord correction factors were used, in accordance with the LR SCF equations, without any independent verification being possible.

The design factor to be applied to these mean fit expressions was also a matter for some debate. No safety factor is suggested for SCF equations in the HSE guidance notes (12), and consequently some SCF equations are designed to be a mean fit to the associated experimental dataset, while some are designed to be a characteristic fit. Overall, it was felt that SCF equations that are currently used in offshore tubular joint design have an appropriate level of safety. This rather subjective view of the reliability of SCF prediction led LR to multiply the mean fit equation, by one standard deviation of this mean fit to the LR database. This led to a design equation that underestimated around 15% to 20% of results.

The LR SCF derivation database is almost identical to the SCF assessment database that was used in this project to assess SCF formulae. It includes both steel and acrylic joint data, and has the same parametric and geometric limitations. Therefore, comparisons between the SCF assessment database and the LR equations should be considered in the knowledge that the equations themselves were largely developed from the dataset against which they are being compared.

With regard to the LR equations, the following points should be noted:

(i) These equations generally give the SCF at the saddle and crown locations (except for IPB), and may underestimate a larger SCF if located between these locations. This is most likely to be the case for K/KT joints under axial load, although it was considered that the differences would be small.
The LR equations use the Efthymiou short chord correction factors, which have not been independently verified.

The LR equations are limited to $\gamma$ ratios greater than $\gamma = 12$, while a significant number of tubular joints are designed with $\gamma$ values below this limitation.

Short chord length effects, chord bending effects and the weld influence have been considered in deriving these equations.

The form of the equations, while being more complex for ‘hand calculations’, gives a more logical influence function format, which largely removes the problem of joint classification.

5. ASSESSMENT OF SIMPLE JOINT SCF EQUATIONS

Throughout the duration and subsequent to the completion of this project, a considerable amount of discussion has centred on defining the characteristics of a ‘good’ or ‘poor’ SCF parametric equation.

While it is clear that an equation is superior if it exhibits less scatter with respect to test data for all combinations of parametric values, acceptable levels of underprediction have not been defined. Unlike the current tubular joint S-N fatigue design curve where a 2.5% probability of failure is predicted, no specific probability level is currently defined for SCF equations. It was found that the parametric equations generally underpredict around 0% - 40% of measured SCF results depending upon the equation/joint configuration/loadcase/measuring location considered.

The equations derived by Kuang, W/S, UEG and HCD are based on either acrylic model test results or on the results of thin shell FE analyses. In both these methods of analysis, no weld fillets were described and consequently the test results themselves tend to be conservative. Therefore, since equations have been produced, generally by some form of least squares curve fit to these data points, the equations themselves do tend to be conservative relative to steel joint test results.

The Efthymiou equations on the other hand, were based on a least square fit to FE results using 3-D shell elements including a realistic weld fillet. Consequently, the Efthymiou equations frequently give the closest agreement with steel joint test results in terms of scatter, but are generally less conservative than other parametric equations.

The HSE Review Panel for Fatigue Guidance (RPFG) considered that the new fatigue guidance should give recommendations regarding the applicability of SCFs. The panel was seeking to give a clear definition of the requirements of an acceptable parametric equation for simple tubular T/Y, X and K joints (13). This definition takes into account the scatter of the equation with respect to measured SCF results, the number of SCF underpredictions and to a lesser extent the number of significant SCF overpredictions.

The LR SCF derivation database described in Section 3 was refined and used to assess the applicability of the more commonly used SCF parametric equations for simple joints. From this assessment a series of matrices for each joint type and...
loading case was produced. There was insufficient data to produce a matrix for KT joints.

5.1 ASSESSMENT DATABASE

The decision to compare parametric equations with a combined database of steel and acrylic joints led to a great deal of debate over the validity of this approach. With regard to the measurement of SCFs, steel data was known to be limited in geometric range, applied loadcases and in location of strain gauges. Additionally, the steel data was potentially biased due to the short chord lengths of the specimens and the proportionally large welds modelled. The short chord length and large weld fillet effects would both tend to reduce the measured SCFs, thus leading to possible underestimation of tubular joint stresses in practice. Furthermore, some anomalies had been noted in the design SCF equations when compared to acrylic model test results which would not be noted if only steel joint data was considered. However, acrylic joints are not steel joints, they merely represent steel models. Acrylic models do not include weld fillets and consequently, approximations had to be made to represent the effect of the weld fillet.

It was agreed that the ideal position would be to use acrylic models/finite element analyses to identify the areas of potential underestimation, and model these joints in steel. However, it was clear that a substantial number of steel models would be required given the combinations of joint configuration, loadcase and measuring positions to be assessed. It was felt to be more important to highlight potentially unsafe SCF design equations given the aforementioned limitations, than remove acrylic models from the assessment of parametric equations. Comparisons of the steel and acrylic model subsets within the database were performed to assess potential differences between the modelling techniques.

Initially, direct comparisons were sought for nominally identical joints tested in both steel and acrylic. Unfortunately, very few acrylic and steel tests were performed under the same conditions, and often small differences in the parametric values are reflected in differences between measured SCF values. A direct comparison of one joint tested in steel, acrylic and using 2 FE analyses (11) showed generally good agreement between all the methods employed, once differences in the experimental techniques and the inclusion of a weld fillet were considered, see Table 5.1.

<table>
<thead>
<tr>
<th>Loading</th>
<th>Location on joint</th>
<th>Steel UKOSRP II</th>
<th>Acrylic model</th>
<th>Wimpey FE</th>
<th>KSEPL FE</th>
</tr>
</thead>
<tbody>
<tr>
<td>Axial load Chord Saddle</td>
<td>24.2</td>
<td>23.9</td>
<td>21.9</td>
<td>23.5</td>
<td></td>
</tr>
<tr>
<td>Chord Crown</td>
<td>8.1</td>
<td>7.0</td>
<td>6.8</td>
<td>7.3</td>
<td></td>
</tr>
<tr>
<td>Brace Saddle</td>
<td>14.1</td>
<td>13.5</td>
<td>15.0</td>
<td>12.7</td>
<td></td>
</tr>
<tr>
<td>Brace Crown</td>
<td>1.8</td>
<td>1.6</td>
<td>1.5</td>
<td>-</td>
<td></td>
</tr>
<tr>
<td>OPB Chord Saddle</td>
<td>22.0</td>
<td>24.2</td>
<td>21.0</td>
<td>21.1</td>
<td></td>
</tr>
<tr>
<td>Brace Saddle</td>
<td>13.4</td>
<td>14.0</td>
<td>14.9</td>
<td>13.2</td>
<td></td>
</tr>
<tr>
<td>PB Chord Crown</td>
<td>5.9</td>
<td>6.0</td>
<td>5.4</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Brace Crown</td>
<td>2.8</td>
<td>3.0</td>
<td>2.5</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Chordside</td>
<td>6.3</td>
<td>6.6</td>
<td>6.4</td>
<td>6.9</td>
<td></td>
</tr>
<tr>
<td>Braceside</td>
<td>3.6</td>
<td>4.2</td>
<td>4.0</td>
<td>5.3</td>
<td></td>
</tr>
</tbody>
</table>

(τ = 1.0, β = 0.5, γ = 28.6, a = 5.0, θ = 90°)
During the assessment, comparisons were made between the steel and acrylic joint SCF databases. One example for the chordside of T/Y joints under axial loading is presented in Table 5.2. It can be seen that the average level of SCF overprediction using the equations varies between the steel joints and acrylic joints with the largest difference occurring, for this example, with the Efthymiou equation. The SCFs using the Efthymiou equation exceed the steel joint database SCFs by an average of 7% and the acrylic joint database SCFs by an average of 17% for this particular joint type and loadcase. Although this pattern is repeated for all the equations except Kuang in this example it should be noted that for other joint types/loadcases the situation could be reversed ie the average steel joint P/R exceedence being greater than the average acrylic joint P/R exceedence.

Taking the average steel joint exceedence and the average acrylic joint exceedence for all equations, and for all loadcases, there is less than 3.5% difference in SCF between the steel and acrylic joints. Given the fact that the steel and acrylic joint SCF databases contain difference joint geometries, and that these geometries are not evenly distributed across the range of parameters influencing the level of stress concentration in the joint, it is understandable why differences were noted for given equations. However, it was reassuring to find so little difference between the steel and acrylic joints in their average level of SCF, in comparison to the parametric equations considered.

<table>
<thead>
<tr>
<th>Loading</th>
<th>Equation</th>
<th>No of joints</th>
<th>Steel/Acrylic</th>
<th>Average Predicted SCF</th>
<th>% Standard deviation</th>
<th>% Joints Under-predicting</th>
</tr>
</thead>
<tbody>
<tr>
<td>Chord side</td>
<td>Wordsworth</td>
<td>22 Steel</td>
<td>1.09</td>
<td>9.7</td>
<td>9.1</td>
<td></td>
</tr>
<tr>
<td></td>
<td></td>
<td>58 Acrylic</td>
<td>1.15</td>
<td>17.5</td>
<td>17.2</td>
<td></td>
</tr>
<tr>
<td></td>
<td>UEG</td>
<td>28 Steel</td>
<td>1.12</td>
<td>9.2</td>
<td>7.1</td>
<td></td>
</tr>
<tr>
<td></td>
<td></td>
<td>58 Acrylic</td>
<td>1.16</td>
<td>13.6</td>
<td>13.8</td>
<td></td>
</tr>
<tr>
<td></td>
<td>Efthymiou</td>
<td>28 Steel</td>
<td>1.07</td>
<td>10.6</td>
<td>28.6</td>
<td></td>
</tr>
<tr>
<td></td>
<td></td>
<td>57 Acrylic</td>
<td>1.17</td>
<td>20.2</td>
<td>12.3</td>
<td></td>
</tr>
<tr>
<td></td>
<td>Kuang</td>
<td>14 Steel</td>
<td>0.90</td>
<td>16.3</td>
<td>64.3</td>
<td></td>
</tr>
<tr>
<td></td>
<td></td>
<td>20 Acrylic</td>
<td>0.87</td>
<td>19.9</td>
<td>70.0</td>
<td></td>
</tr>
<tr>
<td></td>
<td>HCD</td>
<td>28 Steel</td>
<td>1.15</td>
<td>18.1</td>
<td>21.4</td>
<td></td>
</tr>
<tr>
<td></td>
<td></td>
<td>50 Acrylic</td>
<td>1.15</td>
<td>24.5</td>
<td>24.0</td>
<td></td>
</tr>
<tr>
<td></td>
<td>LR</td>
<td>28 Steel</td>
<td>1.13</td>
<td>9.1</td>
<td>7.1</td>
<td></td>
</tr>
<tr>
<td></td>
<td></td>
<td>59 Acrylic</td>
<td>1.19</td>
<td>18.7</td>
<td>5.1</td>
<td></td>
</tr>
</tbody>
</table>

The comparison of steel and acrylic joint SCF results can be summarised as follows:

(i) Steel data models alone must give the best comparison with full scale nodal joints.

(ii) Steel joint data tends to cover a more limited number of joint parameters, and is very sparse form some joint configurations and loadcases.
In addition, steel data has intended to be based on joints with relatively short chord lengths and is more prone to the effects of the end diaphragms.

(iii) Acrylic data generally gives good coverage of parametric ranges for most joint configurations. However, factors have to be employed to account for the lack of a weld fillet.

(iv) Acrylic joints gives correlation with steel joints in the few cases where identical specimens have been tested. Global comparisons of the acrylic database and steel databases do show differences in the percentage of joints underpredicting, which on average balance out. These differences are generally explained by different joint configurations held in the databases, however, some differences have not yet been fully understood and explained.

In conclusion, the LR SCF derivation database described in Section 3 was accepted for assessment of SCFs with only minor modification. The main differences were the inclusion of some steel joints tested by UCL and the exclusion of some steel joints tested by Wimpey Offshore which were found to have strain gauges located within the notch zone. The refined database, titled the SCF assessment database, is given in Appendix B. The total numbers of steel and acrylic joint specimens in the database are given in Table 5.3.

<table>
<thead>
<tr>
<th>Joint type</th>
<th>Steel</th>
<th>Acrylic</th>
<th>Total</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>$\beta &lt; 1$</td>
<td>$\beta = 1$</td>
<td>All $\beta$</td>
</tr>
<tr>
<td>T/Y</td>
<td>85</td>
<td>26</td>
<td>111</td>
</tr>
<tr>
<td>X</td>
<td>24</td>
<td>8</td>
<td>32</td>
</tr>
<tr>
<td>K</td>
<td>34</td>
<td>1</td>
<td>35</td>
</tr>
<tr>
<td>Total</td>
<td>143</td>
<td>35</td>
<td>178</td>
</tr>
</tbody>
</table>

### 5.2 CRITERIA FOR ASSESSMENT OF PARAMETRIC EQUATIONS

In describing the criteria used in this project, it should be noted that many attempts to find satisfactory criteria were made, however these generally fell into 2 forms:

(i) Compare the equations to the database to see if predetermined levels of underprediction/overprediction are met, and reject or flag equations that fail to meet these requirements.

(ii) Use the mean and standard deviation of the data to fit probability distributions to the data, thus reducing the significance of individual points that do not fit the norm. Then compare the equations to the probability distributions to see if predetermined levels of underprediction/overprediction are met.

Ultimately option (i) was favoured for the more direct comparison between the measured data values and the parametric equations because it was difficult to identify specific probability distributions using the limited data available.
For determining the performance of an SCF equation against an SCF database, the following aspects were felt to be the most significant:

(i) The percentage of joints underpredicted by a given equation, i.e., % of joints where \( P/R < 1 \) where \( P \) = predicted SCF and \( R \) = recorded SCF.

(ii) The percentage of joints that are significantly underpredicted by a given equation (for this purpose percentage of joints where \( P/R < 0.8 \) was chosen as a guideline).

(iii) The percentage of joints that are significantly overpredicted by a given equation (for this purpose percentage of joints where \( P/R > 1.5 \) was chosen as a guideline). It was felt that it would be useful to know when an equation was generally overconservative, however an equation should not be considered unacceptable because it is overconservative.

(iv) Steel joints should be given priority where sufficient data exists.

After considerable investigation the following criteria were felt to give the best guideline and have been employed in this study:

- If the number of steel joint SCFs in the database \( \leq 20 \) then assess the steel database alone.
  
  If the number of steel joint SCFs in the database \( < 20 \), and the number of pooled joint SCFs (steel joints and acrylic joints) \( \geq 15 \) then assess the pooled database.
  
  If the number of pooled joint SCFs in the database \( < 15 \), then the equation cannot be assessed.

- For the given dataset, if percentage SCFs underpredicting \( \leq 25\% \) (%\( P/R < 1.0 \leq 25\% \)) and if percentage SCFs considerably underpredicting \( \leq 5\% \) (%\( P/R < 0.8 \leq 5\% \)) then the equation meets the acceptance criteria used in this study. If, in addition, the percentage SCFs considerably overpredicting \( \geq 50\% \) (%\( P/R > 1.5 \geq 50\% \)), then note that the equation is generally conservative.

- If the acceptance criteria as used in this study is nearly met (i.e. \( 25\% < \%P/R < 1.0 \leq 30\% \) and/or \( 5\% < \%P/R < 0.8 \leq 7.5\% \)) then the equation is regarded as borderline and engineering judgement has been used.

It is obvious that the values taken in this assessment are subjective, however, these values do meet the objectives of the project, and generally support the equations that are currently in use while identifying some significant anomalies in the parametric equations which should be avoided.
5.3 ASSESSMENT OF PARAMETRIC EQUATIONS

In assessing the parametric equations the following methodology was adopted:

(i) To assess the effect of chord length on saddle SCFs for axial loading and OPB a considerable amount of work was performed. It was concluded that no single $a$ cut-off value could account for the effect of the chord end diaphragms. Consequently, the short chord correction factors proposed by Efthymiou were employed. If an SCF were influenced by more than 10% due to the end conditions (ie SCCF < 0.9) then the SCF was excluded from the assessment.

(ii) All joints were assumed to be pinned at the chord ends.

(iii) Initially, $\beta = 1$ joints were excluded from analyses at the saddle locations, since $\beta = 1$ joints generally give very variable results primarily due to the degree of weld cut-back employed.

(iv) Only joints that have all parameters inside the equation’s recommended range of applicability were included in the analysis. This action had the effect of considerably reducing the number of joints available, particularly for the Kuang equations.

(v) Joints with SCFs less than 1.5 were excluded from the assessment.

(vi) The variable P/R (= Predicted SCF/Recorded SCF) was analysed and found to be approximately lognormally distributed. The mean P/R value, the standard deviation of the variable P/R, % P/R < 0.8, %P/R < 1.0 and the %P/R > 1.5 are assessed for each combination of configuration/loadcase/measuring position/equation.

5.4 PERFORMANCE OF PARAMETRIC EQUATIONS

In Tables 1 - 22 the performance of each equation is described for joint geometries (T/Y, X and K), loadcases (axial, OPB and IPB) and measuring positions (chord saddle, chord crown, brace saddle and brace crown). It should be noted that the HCD equations only cover T/Y joints and the Kuang equations T/Y and K joints. For the acceptance criteria used, the Efthymiou and new LR equations are the most consistent giving the best performance overall.

5.5 DATA USED IN SCF ASSESSMENT

Stress concentration factor matrices for T/Y, X and K joints have been produced in this project, see Tables 5.4 to 5.6. There was insufficient data available to produce a matrix for KT joints.
### Table 5.4
#### T/Y Joints

<table>
<thead>
<tr>
<th>Loading</th>
<th>Position</th>
<th>Kuang</th>
<th>Words</th>
<th>U E G</th>
<th>Efthy</th>
<th>HCD</th>
<th>LR</th>
</tr>
</thead>
<tbody>
<tr>
<td>Axial</td>
<td>Chord Saddle</td>
<td>(34 P)</td>
<td>(22 S)</td>
<td>(28 S)</td>
<td>(26 S)</td>
<td>(28 S)</td>
<td>(28 S)</td>
</tr>
<tr>
<td></td>
<td>Chord Crown</td>
<td>N/A</td>
<td>(43 P)</td>
<td>(43 P)</td>
<td>(48 P)</td>
<td>(29 P)</td>
<td>(48 P)</td>
</tr>
<tr>
<td></td>
<td>Brace Crown</td>
<td>N/A</td>
<td>(30 P)</td>
<td>(30 P)</td>
<td>(35 P)</td>
<td>(19 P)</td>
<td>(35 P)</td>
</tr>
<tr>
<td>OPB</td>
<td>Chordside</td>
<td>(18 P)</td>
<td>(70 P)</td>
<td>(74 P)</td>
<td>(73 P)</td>
<td>(65 P)</td>
<td>(75 P)</td>
</tr>
<tr>
<td></td>
<td>Brace Sides</td>
<td>(13 P)</td>
<td>(44 P)</td>
<td>(47 P)</td>
<td>(48 P)</td>
<td>(42 P)</td>
<td>(48 P)</td>
</tr>
<tr>
<td>IPB</td>
<td>Chordside</td>
<td>(19 P)</td>
<td>(61 P)</td>
<td>(21 S)</td>
<td>(21 S)</td>
<td>(44 P)</td>
<td>(23 S)</td>
</tr>
<tr>
<td></td>
<td>Brace Sides</td>
<td>(19 P)</td>
<td>(44 P)</td>
<td>(24 S)</td>
<td>(24 S)</td>
<td>(35 P)</td>
<td>(26 S)</td>
</tr>
</tbody>
</table>

### Table 5.5
#### X Joints

<table>
<thead>
<tr>
<th>Loading</th>
<th>Position</th>
<th>Words</th>
<th>U E G</th>
<th>Efthy</th>
<th>LR</th>
</tr>
</thead>
<tbody>
<tr>
<td>Balanced</td>
<td>Chord Saddle</td>
<td>(25 P)</td>
<td>(27 P)</td>
<td>(32 P)</td>
<td>(32 P)</td>
</tr>
<tr>
<td></td>
<td>Chord Crown</td>
<td>(12 P)</td>
<td>(12 P)</td>
<td>(12 P)</td>
<td>(12 P)</td>
</tr>
<tr>
<td></td>
<td>Brace Saddle</td>
<td>(16 P)</td>
<td>(16 P)</td>
<td>(16 P)</td>
<td>(16 P)</td>
</tr>
<tr>
<td></td>
<td>Brace Crown</td>
<td>(16 P)</td>
<td>(16 P)</td>
<td>(16 P)</td>
<td>(16 P)</td>
</tr>
<tr>
<td>Balanced</td>
<td>Chordside</td>
<td>(23 P)</td>
<td>(23 P)</td>
<td>(23 P)</td>
<td>(23 P)</td>
</tr>
<tr>
<td></td>
<td>Brace Sides</td>
<td>(16 P)</td>
<td>(16 P)</td>
<td>(16 P)</td>
<td>(16 P)</td>
</tr>
<tr>
<td>Balanced</td>
<td>Chordside</td>
<td>(33 P)</td>
<td>(33 P)</td>
<td>(36 P)</td>
<td>(36 P)</td>
</tr>
<tr>
<td></td>
<td>Brace Sides</td>
<td>(27 P)</td>
<td>(27 P)</td>
<td>(29 P)</td>
<td>(28 P)</td>
</tr>
</tbody>
</table>

### Table 5.6
#### K Joints

<table>
<thead>
<tr>
<th>Loading</th>
<th>Position</th>
<th>Kuang</th>
<th>Words</th>
<th>UEG</th>
<th>Efthy</th>
<th>LR</th>
</tr>
</thead>
<tbody>
<tr>
<td>Balanced</td>
<td>Chord Saddle</td>
<td>(16 P)</td>
<td>(54 P)</td>
<td>(54 P)</td>
<td>(25 S)</td>
<td>(24 S)</td>
</tr>
<tr>
<td></td>
<td>Brace Sides</td>
<td>(13 P)</td>
<td>(46 P)</td>
<td>(46 P)</td>
<td>(23 S)</td>
<td>(21 S)</td>
</tr>
<tr>
<td>Unbalanced</td>
<td>Chordside</td>
<td>N/A</td>
<td>(50 P)</td>
<td>(50 P)</td>
<td>(50 P)</td>
<td>(50 P)</td>
</tr>
<tr>
<td></td>
<td>Brace Sides</td>
<td>N/A</td>
<td>(39 P)</td>
<td>(39 P)</td>
<td>(39 P)</td>
<td>(39 P)</td>
</tr>
<tr>
<td>Balanced</td>
<td>Chordside</td>
<td>(6 A)</td>
<td>(32 A)</td>
<td>(32 A)</td>
<td>(32 A)</td>
<td>(32 A)</td>
</tr>
<tr>
<td></td>
<td>Brace Sides</td>
<td>(7 A)</td>
<td>(34 A)</td>
<td>(34 A)</td>
<td>(34 A)</td>
<td>(34 A)</td>
</tr>
</tbody>
</table>

Key to SCF TABLES:

Basis of the assessment in accordance with the criteria given in Section 5.2:

- **()**: Number of joints in the database and SCF database used.
- **s**: The equation has been assessed against the steel joint database alone (ie n_{steel}>=20)
- **p**: The equation has been assessed against the ‘pooled’ steel and acrylic joint database.
- **A**: The equation has been assessed against the acrylic joint database.
- **N/A**: There is no parametric equation for this loadcase.
5.6 BETA = 1.0 JOINTS

Joints with equal brace and chord diameters (ie $\beta = 1.0$) were excluded from the main assessment at the saddle location. For $\beta = 1.0$ acrylic models the factors applied with respect to extrapolation, conversion from SNCF to SCF and weld fillet effects were very different from those for $\beta \neq 1.0$ joints. The factors were also very inconsistent within the $\beta = 1.0$ dataset itself.

At the saddle location for $\beta = 1.0$ joints the SCF is very significantly influenced by the degree of cut-back, see paragraph 2.5.2. In the JISSP study for $\beta = 1.0$ X joints there were significant differences in quoted SCFs resulting from linear and non-linear extrapolation.

However, $\beta = 1.0$ joints have been analysed separately to account for the wide variation in the degree of cut-back at the saddle. A number of $\beta$ modification factors have been derived which should be used in conjunction with equations which meet the acceptance criteria, used in this study, at the saddle location.

For T/Y joints the $\beta$ modification factors are given in Table 23 and for X joints in Table 24. These factors should be applied to those equations which meet the criteria in Tables 1 to 22 for $\beta = 1.0$ joints. These factors have been selected to ensure that the equations accepted for the $\beta = 1.0$ range can be applied to $\beta = 1.0$ joints to meet the criteria used in this study. No modification factors can be derived for the K joints because of the lack of suitable data (less than 10 joints of this type).

6. CONCLUSIONS

In this report, 2 studies largely funded by the HSE, have been described. The first study led to the creation of a comprehensive database of steel and acrylic joint SCFs titled the Lloyd’s Register SCF derivation database and described in Section 3.0.

From this database a new set of SCF parametric equations was derived from simple tubular nodal joints. These equations are referred to as the new LR equations and were derived as a mean fit to the database. A one standard deviation safety factor was included to give the design equations, see Appendix A.

In the second study the database was refined and used to assess existing simple joint SCF parametric formulæ including the new LR equations. The finalised database, titled the SCF assessment database and given in Appendix B, contains 191 T/Y, 64 X and 67 K joints. The database was screened and for the assessment all joints failing to meet the specified criteria were excluded.

In the assessment of existing SCF parametric equations, the objective was to produce SCF matrices giving recommendations regarding the use of the equations for new HSE fatigue guidance. The assessment criteria was agreed by the Review Panel for Fatigue Guidance and the performance of the equations is presented in Tables 1 to 22.

In determining which SCF equations should be used, the allowable equations should be treated with caution for 2 reasons. Firstly, the designer may feel, with some justification, that since a number of SCF equations are acceptable for a given loadcase, the minimum SCF from these acceptable equations could be taken.
Secondly, ‘mixing’ equations to give one allowable set of equations may lead to problems if in future the influence function approach is adopted rather than the current rather vague joint classification approach. The influence function approach relies on an understanding of the effect of the presence and loading of individual braces and the change in SCF that results.

ACKNOWLEDGEMENTS

The authors wish to thank their colleagues at Lloyd’s Register, Dr J Sharp of the HSE and N Nichols of MaTSU for their valuable contributions during this project, and to the HSE for providing the major funding for the work described in this report.

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Edinburgh, UK, 1991
Table 1
T/Y joints - Axial - Chord saddle - ($\beta<1$; SCCF $\geq 0.9$; SCF $\geq 1.5$)

<table>
<thead>
<tr>
<th>Equation</th>
<th>Steel/ acrylic</th>
<th>No of Pts</th>
<th>Database</th>
<th>Pred SCF/Recorded SCF</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Mean (Pa)</td>
<td>%st dev of Equn</td>
<td>% P/R &lt;0.8</td>
<td>% P/R &lt;1.0</td>
</tr>
<tr>
<td>Kuang Steel</td>
<td>0.90</td>
<td>16.3%</td>
<td>28.6%</td>
<td>64.3%</td>
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<td>Acrylic</td>
<td>0.88</td>
<td>19.7%</td>
<td>25.0%</td>
<td>70.0%</td>
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<tr>
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<td>0.0%</td>
<td>9.1%</td>
</tr>
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<td>Acrylic</td>
<td>1.13</td>
<td>13.9%</td>
<td>0.0%</td>
<td>17.2%</td>
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<tr>
<td>Pooled</td>
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<td>12.9%</td>
<td>0.0%</td>
<td>15.0%</td>
</tr>
<tr>
<td>UEG Steel</td>
<td>1.12</td>
<td>9.2%</td>
<td>0.0%</td>
<td>7.1%</td>
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<tr>
<td>Acrylic</td>
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<td>13.5%</td>
<td>0.0%</td>
<td>13.8%</td>
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<td>15.3%</td>
<td>0.0%</td>
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Table 2
T/Y joints - Axial - Chord Crown - (C = 1; SCCF $\geq 0.9$; SCF $\geq 1.5$)

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<th>Equation</th>
<th>Steel/ acrylic</th>
<th>No of Pts</th>
<th>Database</th>
<th>Pred SCF/Recorded SCF</th>
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<td>Mean (Pa)</td>
<td>%st dev of Equn</td>
<td>% P/R &lt;0.8</td>
<td>% P/R &lt;1.0</td>
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<td>4.7%</td>
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<td>22.2%</td>
<td>33.3%</td>
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<td>0.0%</td>
<td>22.2%</td>
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<tr>
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<td>17.8%</td>
<td>0.0%</td>
<td>10.3%</td>
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<td>3.4%</td>
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<td>No of Pts</td>
<td>Database</td>
<td>Pred SCF/Recorded SCF</td>
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<td>% P/R &lt;0.8</td>
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<td>25.6%</td>
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<td>25.1%</td>
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<td>36.5%</td>
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<td>20.5%</td>
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<td>48</td>
<td>1.18</td>
<td>20.7%</td>
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</table>
### Table 4
**T/Y joints - Axial - Brace Crown - (C = 1; SCCF ≥ 0.9; SCF ≥ 1.5)**

<table>
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<tr>
<th>Equation</th>
<th>Steel/ acrylic</th>
<th>No of Pts</th>
<th>Database Mean</th>
<th>%st dev of Equn</th>
<th>Pred SCF/Recorded SCF</th>
<th>% P/R &lt;0.8</th>
<th>% P/R &lt;1.0</th>
<th>% P/R &gt;1.5</th>
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<td>2.38</td>
<td>68.7%</td>
<td>0.0% 0.0% 92.3%</td>
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<td></td>
<td>Pooled</td>
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<td>67.5%</td>
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<tr>
<td>UEG</td>
<td>Steel</td>
<td>4</td>
<td>1.92</td>
<td>48.2%</td>
<td>0.0% 0.0% 75.0%</td>
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<td></td>
<td>Acrylic</td>
<td>26</td>
<td>2.38</td>
<td>68.3%</td>
<td>0.0% 0.0% 92.3%</td>
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### Table 5
**T/Y joints - OPB - Chordside - (β<1; SCCF ≥ 0.9; SCF ≥ 1.5)**

<table>
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<tr>
<th>Equation</th>
<th>Steel/ acrylic</th>
<th>No of Pts</th>
<th>Database Mean</th>
<th>%st dev of Equn</th>
<th>Pred SCF/Recorded SCF</th>
<th>% P/R &lt;0.8</th>
<th>% P/R &lt;1.0</th>
<th>% P/R &gt;1.5</th>
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<td></td>
<td>Acrylic</td>
<td>12</td>
<td>0.91</td>
<td>7.5%</td>
<td>16.7% 100% 0.0%</td>
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<td>Pooled</td>
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<td>7.7%</td>
<td>22.2% 100% 0.0%</td>
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</tr>
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<td>Wordsworth &amp; Smedley</td>
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<td>5.6% 22.2% 0.0%</td>
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<td>13.4%</td>
<td>0.0% 22.2% 0.0%</td>
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<td>24.7%</td>
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T/Y joints - OPB - Braceside - (β<1; SCCF≥0.9; SCF≥1.5)

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**T/Y joints - IPB - Chordside - (SCF≥1.5)**

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### Table 8
**T/Y joints - IPB - Braceside - (SCF≥1.5)**

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### Table 9

**X joints - Axial - Chord Saddle - (β<1; SCF≥1.5)**

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### Table 10

**X joints - Axial - Chord Crown - (C = 1; SCF≥1.5)**

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### Table 11

**X joints - Axial - Brace Saddle - (β<1; SCF≥1.5)**

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<td>Database</td>
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Table 12
X joints - Axial - Brace Crown - (C = 1; SCF ≥1.5)

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Table 13
X joints - OPB - Chordside - (β<1; SCF ≥1.5)

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### Table 14
**X joints - OPB - Braceside - \( (\beta < 1; \ SCF \geq 1.5) \)**

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### Table 15
**X joints - IPB - Chordside - (SCF \geq 1.5)**

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<th>No of Pts</th>
<th>Database</th>
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<th>Mean</th>
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<th>% P/R &lt; 1.0</th>
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### Table 16
**X joints - IPB - Braceside - (SCF \geq 1.5)**

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<td>Steel</td>
<td>19</td>
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<td>10.5%</td>
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<td></td>
</tr>
<tr>
<td></td>
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<td>35</td>
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<td>28.3%</td>
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<td>37.1%</td>
<td>8.6%</td>
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</tr>
<tr>
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<td>9.3%</td>
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</tr>
<tr>
<td>Efthymiou</td>
<td>Steel</td>
<td>25</td>
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</tr>
<tr>
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<td>20.0%</td>
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</tr>
<tr>
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</tr>
<tr>
<td></td>
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<td>8.5%</td>
<td>40.7%</td>
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</tr>
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</table>

Table 17
K joints - Bal axial load - Chordside - ($\beta < 1$; SCCF ≤ 0.9; SCCF ≥ 1.5)

<table>
<thead>
<tr>
<th>Equation</th>
<th>Steel/ acrylic</th>
<th>No of Pts</th>
<th>Database</th>
<th>Pred SCF/Recorded SCF</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td></td>
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<td>%st dev of Eqn</td>
</tr>
<tr>
<td>Kuang</td>
<td>Steel</td>
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<td>1.33</td>
<td>10.7%</td>
</tr>
<tr>
<td></td>
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<td>28.7%</td>
</tr>
<tr>
<td></td>
<td>Pooled</td>
<td>13</td>
<td>1.36</td>
<td>20.3%</td>
</tr>
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</tr>
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<td>1.29</td>
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<td>37.3%</td>
</tr>
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<td>UEG</td>
<td>Steel</td>
<td>15</td>
<td>1.41</td>
<td>43.5%</td>
</tr>
<tr>
<td></td>
<td>Acrylic</td>
<td>31</td>
<td>1.31</td>
<td>33.7%</td>
</tr>
<tr>
<td></td>
<td>Pooled</td>
<td>46</td>
<td>1.34</td>
<td>37.0%</td>
</tr>
<tr>
<td>Efthymiou</td>
<td>Steel</td>
<td>23</td>
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<td>Steel</td>
<td>21</td>
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</tr>
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</table>

Table 18
K joints - Bal axial load - Braceside - ($\beta < 1$; SCCF ≤ 0.9; SCCF ≥ 1.5)
<table>
<thead>
<tr>
<th>Equation</th>
<th>Steel/ acrylic</th>
<th>No of Pts</th>
<th>Database Mean</th>
<th>%P/R &lt;0.8</th>
<th>%P/R &lt;1.0</th>
<th>%P/R &gt;1.5</th>
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<td></td>
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<td>3.8%</td>
<td>7.7%</td>
<td>34.6%</td>
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<tr>
<td></td>
<td>Acrylic</td>
<td>31</td>
<td>1.39</td>
<td>25.2%</td>
<td>3.2%</td>
<td>35.5%</td>
</tr>
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</table>

Table 19
K joints - Unbal OPB - Chordside - ($\beta<1$; SCCF$\leq0.9$; SCF$\geq1.5$)

<table>
<thead>
<tr>
<th>Equation</th>
<th>Steel/ acrylic</th>
<th>No of Pts</th>
<th>Database Mean</th>
<th>%P/R &lt;0.8</th>
<th>%P/R &lt;1.0</th>
<th>%P/R &gt;1.5</th>
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</thead>
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<td></td>
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<td>1.31</td>
<td>2.0%</td>
<td>2.0%</td>
<td>18.0%</td>
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<tr>
<td></td>
<td>Acrylic</td>
<td>35</td>
<td>1.26</td>
<td>15.7%</td>
<td>2.9%</td>
<td>11.4%</td>
</tr>
<tr>
<td></td>
<td>Steel</td>
<td>15</td>
<td>1.41</td>
<td>18.5%</td>
<td>0.0%</td>
<td>33.3%</td>
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</table>

<table>
<thead>
<tr>
<th>Equation</th>
<th>Steel/ acrylic</th>
<th>No of Pts</th>
<th>Database Mean</th>
<th>%P/R &lt;0.8</th>
<th>%P/R &lt;1.0</th>
<th>%P/R &gt;1.5</th>
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</thead>
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<td>23.0%</td>
<td>4.0%</td>
<td>8.0%</td>
</tr>
<tr>
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<td>Acrylic</td>
<td>35</td>
<td>1.08</td>
<td>22.2%</td>
<td>5.7%</td>
<td>8.6%</td>
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<tr>
<td></td>
<td>Steel</td>
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<td>0.0%</td>
<td>6.7%</td>
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<th>Steel/ acrylic</th>
<th>No of Pts</th>
<th>Database Mean</th>
<th>%P/R &lt;0.8</th>
<th>%P/R &lt;1.0</th>
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<td>1.13</td>
<td>14.6%</td>
<td>0.0%</td>
<td>2.9%</td>
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<tr>
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<td>Steel</td>
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<td>13.7%</td>
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<td>0.0%</td>
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Table 20
K joints - Unbal OPB - Braceside - ($\beta<1$; SCCF$\leq0.9$; SCF$\geq1.5$)

<table>
<thead>
<tr>
<th>Equation</th>
<th>Steel/ acrylic</th>
<th>No of Pts</th>
<th>Database Mean</th>
<th>%P/R &lt;0.8</th>
<th>%P/R &lt;1.0</th>
<th>%P/R &gt;1.5</th>
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<td>66.7%</td>
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<tr>
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<td>Acrylic</td>
<td>31</td>
<td>1.61</td>
<td>33.8%</td>
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<td>61.3%</td>
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<tr>
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<td>Steel</td>
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<td>1.95</td>
<td>44.7%</td>
<td>0.0%</td>
<td>87.5%</td>
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<table>
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<tr>
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<th>Database Mean</th>
<th>%P/R &lt;0.8</th>
<th>%P/R &lt;1.0</th>
<th>%P/R &gt;1.5</th>
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</thead>
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<td>37.1%</td>
<td>0.0%</td>
<td>25.7%</td>
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<td>Steel</td>
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<td>1.44</td>
<td>36.5%</td>
<td>0.0%</td>
<td>50.0%</td>
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<table>
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<tr>
<th>Equation</th>
<th>Steel/ acrylic</th>
<th>No of Pts</th>
<th>Database Mean</th>
<th>%P/R &lt;0.8</th>
<th>%P/R &lt;1.0</th>
<th>%P/R &gt;1.5</th>
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<td>31</td>
<td>1.26</td>
<td>19.9%</td>
<td>0.0%</td>
<td>9.7%</td>
</tr>
<tr>
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<td>Steel</td>
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<td>1.44</td>
<td>36.5%</td>
<td>0.0%</td>
<td>50.0%</td>
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### Table 21
**K joints - Bal IPB - Chordside - (SCF ≥ 1.5)**

<table>
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<th>Equation</th>
<th>Steel/ acrylic</th>
<th>No of Pts</th>
<th>Database</th>
<th>Pred SCF/Recorded SCF</th>
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</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td></td>
<td>Mean</td>
<td>%st dev of</td>
</tr>
<tr>
<td>Kuang</td>
<td>Steel</td>
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<td>-</td>
<td>-</td>
</tr>
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<td>6</td>
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<td>8.5%</td>
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<tr>
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<td>Pooled</td>
<td>6</td>
<td>0.94</td>
<td>8.5%</td>
</tr>
<tr>
<td>Wordsworth</td>
<td>Steel</td>
<td>0</td>
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<td>-</td>
</tr>
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<td>1.26</td>
<td>26.9%</td>
</tr>
<tr>
<td></td>
<td>Pooled</td>
<td>32</td>
<td>1.26</td>
<td>26.9%</td>
</tr>
<tr>
<td>UEG</td>
<td>Steel</td>
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<td>-</td>
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<td>1.27</td>
<td>26.7%</td>
</tr>
<tr>
<td></td>
<td>Pooled</td>
<td>32</td>
<td>1.27</td>
<td>26.7%</td>
</tr>
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<td>Efthymiou</td>
<td>Steel</td>
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<td>-</td>
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<td>Pooled</td>
<td>32</td>
<td>1.07</td>
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<tr>
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</tr>
<tr>
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<td>1.17</td>
<td>17.3%</td>
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</table>

### Table 22
**K joints - Bal IPB - Braceside - (SCF ≥ 1.5)**

<table>
<thead>
<tr>
<th>Equation</th>
<th>Steel/ acrylic</th>
<th>No of Pts</th>
<th>Database</th>
<th>Pred SCF/Recorded SCF</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td></td>
<td>Mean</td>
<td>%st dev of</td>
</tr>
<tr>
<td>Kuang</td>
<td>Steel</td>
<td>0</td>
<td>-</td>
<td>-</td>
</tr>
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<td>Acrylic</td>
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<td>1.31</td>
<td>27.8%</td>
</tr>
<tr>
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<td>Pooled</td>
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<td>1.31</td>
<td>27.8%</td>
</tr>
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<td>1.76</td>
<td>43.7%</td>
</tr>
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<td>UEG</td>
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<td>-</td>
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<td>1.77</td>
<td>43.3%</td>
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<tr>
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<td>1.77</td>
<td>43.3%</td>
</tr>
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<td>Steel</td>
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<td>1.57</td>
<td>31.3%</td>
</tr>
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<td>LR</td>
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<td>-</td>
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<td>Acrylic</td>
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<td>Pooled</td>
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<td>1.10</td>
<td>18.3%</td>
</tr>
</tbody>
</table>
### Table 23
**T/Y joints $\beta = 1.0$**

<table>
<thead>
<tr>
<th>Loading</th>
<th>Location No joints (P)</th>
<th>Wordsworth/Smedley Beta mod</th>
<th>Efthymiou Beta mod</th>
<th>UEG Beta mod</th>
<th>LR Beta* mod</th>
</tr>
</thead>
<tbody>
<tr>
<td>Axial</td>
<td>Chordside (9)</td>
<td>0.94</td>
<td>0.97</td>
<td>0.98</td>
<td>0.94</td>
</tr>
<tr>
<td></td>
<td>Braceside (16)</td>
<td>0.98</td>
<td>0.97</td>
<td>0.98</td>
<td>0.95</td>
</tr>
<tr>
<td>OPB</td>
<td>Chordside (25)</td>
<td>0.94</td>
<td>0.98</td>
<td>0.98</td>
<td>0.99</td>
</tr>
<tr>
<td></td>
<td>Braceside (26)</td>
<td>0.98</td>
<td>0.96</td>
<td>0.98</td>
<td>0.96</td>
</tr>
</tbody>
</table>

*Note: for LR the Beta mod value quoted is that to be used in the LR equation ie $\beta = \beta_{\text{mod}} - (\tau \sin^{0.65}(\Psi))$

Where: $\Psi$ is the degree of cut-back at saddle, default value = 20° if cutback not known

$$\tau = \frac{\text{thickness of brace}}{\text{thickness of chord}}$$

$$\gamma = \frac{\text{diameter of chord}}{2 \times \text{thickness of chord}}$$

### Table 24
**X joints $\beta = 1.0$**

<table>
<thead>
<tr>
<th>Loading</th>
<th>Location No joints (P)</th>
<th>Wordsworth/Smedley Beta mod</th>
<th>Efthymiou Beta mod</th>
<th>UEG Beta mod</th>
<th>LR Beta* mod</th>
</tr>
</thead>
<tbody>
<tr>
<td>Balanced Axial</td>
<td>Chordside (16)</td>
<td>0.98</td>
<td>1.0</td>
<td>0.98</td>
<td>0.99</td>
</tr>
<tr>
<td></td>
<td>Braceside (16)</td>
<td>0.98</td>
<td>0.98</td>
<td>0.98</td>
<td>0.99</td>
</tr>
<tr>
<td>Balanced OPB</td>
<td>Chordside (13)</td>
<td>0.98</td>
<td>1.0</td>
<td>0.98</td>
<td>0.99</td>
</tr>
<tr>
<td></td>
<td>Braceside (11)</td>
<td>0.98</td>
<td>0.96</td>
<td>0.98</td>
<td>0.99</td>
</tr>
</tbody>
</table>

*Note: for LR the Beta mod value quoted is that to be used in the LR equation ie $\beta = \beta_{\text{mod}} - (\tau \sin^{0.65}(\Psi))$

Where: $\Psi$ is the degree of cut-back at saddle, default value = 20° if cutback not known

$$\tau = \frac{\text{thickness of brace}}{\text{thickness of chord}}$$

$$\gamma = \frac{\text{diameter of chord}}{2 \times \text{thickness of chord}}$$
APPENDIX A
PARAMETRIC EQUATIONS USED IN THIS STUDY
INDEX

A1   LLOYD’S REGISTER PARAMETRIC EQUATIONS
    A1.1 Derivation of Equations and Safety Factors
    A1.2 Derived Parametric Equations and Measured SCF Values
    A1.3 Lloyd’s Register Equations for T/Y Joints
    A1.4 Lloyd’s Register Equations for X Joints
    A1.5 Lloyd’s Register K Joint Equations
    A1.6 Lloyd’s Register KT Joint Equations
    A1.7 Parametric Equation Expressions

A2   EFTHYMIOU PARAMETRIC EQUATIONS
    A2.1 Efthymiou Equations for T/Y Joints
    A2.2 Efthymiou Equations for X Joints
    A2.3 Efthymiou Equations for K Joints
    A2.4 Efthymiou Equations for KT Joints
A1. LLOYD’S REGISTER PARAMETRIC EQUATIONS

A1.1 Derivation of Equations and Safety Factors

These equations have been derived from the Lloyd’s Register (LR) simple joint SCF database using a least squares minimisation procedure. The quoted equations give an approximately mean fit to the database, however characteristic equations can be derived by applying an appropriate safety factor.

Beside each equation is the percentage standard deviation of the least squares fit to the database ($r\%$), which can be utilised to calculate the required safety factor. It has been found that the LR equations are lognormally distributed about the mean fit line. Therefore, the design curve weighting may be calculated to give an estimated degree of joint underpredictions.

$$\text{SCF (design)} = \text{SCF (mean)} \times (1 + \eta \frac{r}{100})$$

Where $\eta = \text{Chosen design curve weighting}$

$\dagger$ Exclude the chord in place bending term, $B_0 \times B_1$ and add in expression unfactored.
A1.2 Derived Parametric Equations and Measured SCF Values

The SCF database was standardised to DEn fatigue recommendations prior to curve fitting (ie to results derived from linear extrapolation of maximum principal stresses outside the $0.2\sqrt{\text{rt}}$ notch zone to the weld toe). Measured SCF results that do not meet this standard should be factored as follows:

- X 0.95 to convert from non-linear extrapolation of stresses
- X 0.95 to simulate a weld fillet on the chordside
- X 0.86 to simulate a weld fillet on the braceside
- X 1.15 to convert perpendicular strain to principal stress (steel models)
- X 1.23 to convert perpendicular strains to principal stress (acrylic models)
A1.3 Lloyd’s Register Equations for T/Y Joints

Notes: When $\alpha < 12$ the basic saddle SCF equation should be multiplied by the appropriate short chord correction factor $F_1, F_2$ etc.

Apply the modified $\beta$ value when predicting SCFs at the SADDLE on $\beta = 1$ joints under axial load or OPB.

### Axial load

<table>
<thead>
<tr>
<th>SCF</th>
<th>Equation</th>
<th>Standard Deviation</th>
</tr>
</thead>
<tbody>
<tr>
<td>$\text{SCF}_{CS}$</td>
<td>$T_1 \times (F_1 \text{ or } F_2)$</td>
<td>$\sigma = 20%$</td>
</tr>
<tr>
<td>$\text{SCF}_{CC}$</td>
<td>$T_2 + B_0 \times B_1$</td>
<td>$\sigma = 20%$</td>
</tr>
<tr>
<td>$\text{SCF}_{BS}$</td>
<td>$T_3 \times (F_1 \text{ or } F_2)$</td>
<td>$\sigma = 25%$</td>
</tr>
<tr>
<td>$\text{SCF}_{BC}$</td>
<td>$T_4$</td>
<td>$\sigma = 23%$</td>
</tr>
</tbody>
</table>

### Out-of-plane bending

<table>
<thead>
<tr>
<th>SCF</th>
<th>Equation</th>
<th>Standard Deviation</th>
</tr>
</thead>
<tbody>
<tr>
<td>$\text{SCF}_{CS}$</td>
<td>$T_5 \times (F_3)$</td>
<td>$\sigma = 22%$</td>
</tr>
<tr>
<td>$\text{SCF}_{BS}$</td>
<td>$T_6 \times (F_3)$</td>
<td>$\sigma = 28%$</td>
</tr>
</tbody>
</table>

### In-plane bending

<table>
<thead>
<tr>
<th>SCF</th>
<th>Equation</th>
<th>Standard Deviation</th>
</tr>
</thead>
<tbody>
<tr>
<td>$\text{SCF}_{C}$</td>
<td>$T_7$</td>
<td>$\sigma = 15%$</td>
</tr>
<tr>
<td>$\text{SCF}_{B}$</td>
<td>$T_8$</td>
<td>$\sigma = 18%$</td>
</tr>
</tbody>
</table>

**Validity range**

The above equations for T/Y joints are generally valid for joint parameters within the following limits:

- $0.13 \leq \beta \leq 1.0$
- $10 \leq \gamma \leq 35$
- $0.25 \leq \tau \leq 1.0$
- $30^\circ \leq \theta \leq 90^\circ$
- $4 \leq a$

Note: $\beta = 1$ joints at the saddle: $\beta = 1 - \left( \frac{\gamma}{7} \times \sin^{0.65} (\Psi^\circ) \right)$

(where $\Psi^\circ$ is the degree of weld cut-back (default value = $20^\circ$)).
A1.4 Lloyd’s Register Equations for X Joints

Notes: When $\alpha < 12$ the basic saddle SCF equation should be multiplied by the appropriate short chord correction factor $F_1, F_2$ etc.

Apply the modified $\beta$ value when predicting SCFs at the SADDLE on $\beta = 1$ joints under axial load or OPB.

<table>
<thead>
<tr>
<th>SCF</th>
<th>Equation</th>
<th>Std Devn</th>
</tr>
</thead>
<tbody>
<tr>
<td>$\text{SCF}_{CS}$</td>
<td>$X_1 \times (F_1$ or $F_2)$</td>
<td>$\sigma = 22%$</td>
</tr>
<tr>
<td>$\text{SCF}_{CC}$</td>
<td>$X_2$</td>
<td>$\sigma = 33%$</td>
</tr>
<tr>
<td>$\text{SCF}_{BS}$</td>
<td>$X_3 \times (F_1$ or $F_2)$</td>
<td>$\sigma = 19%$</td>
</tr>
<tr>
<td>$\text{SCF}_{BC}$</td>
<td>$X_4$</td>
<td>$\sigma = 13%$</td>
</tr>
</tbody>
</table>

Balanced out-of-plane bending

<table>
<thead>
<tr>
<th>SCF</th>
<th>Equation</th>
<th>Std Devn</th>
</tr>
</thead>
<tbody>
<tr>
<td>$\text{SCF}_{CS}$</td>
<td>$X_5 \times F_3$</td>
<td>$\sigma = 22%$</td>
</tr>
<tr>
<td>$\text{SCF}_{BS}$</td>
<td>$X_6 \times F_3$</td>
<td>$\sigma = 20%$</td>
</tr>
</tbody>
</table>

Balanced in-plane bending

<table>
<thead>
<tr>
<th>SCF</th>
<th>Equation</th>
<th>Std Devn</th>
</tr>
</thead>
<tbody>
<tr>
<td>$\text{SCF}_{C}$</td>
<td>$X_7$</td>
<td>$\sigma = 23%$</td>
</tr>
<tr>
<td>$\text{SCF}_{B}$</td>
<td>$X_8$</td>
<td>$\sigma = 12%$</td>
</tr>
</tbody>
</table>

Validity range

The above equations are generally valid for joint parameters within the following limits:

- $0.13 \leq \beta \leq 1.0$
- $10 \leq \gamma \leq 35$
- $0.25 \leq \tau \leq 1.0$
- $30^\circ \leq \theta \leq 90^\circ$
- $4 \leq a$

Note: $\beta = 1$ joints at the saddle: $\beta = 1 - (\frac{\gamma}{\Psi} \times \sin^{0.65}(\Psi^\circ))$

(where $\Psi^\circ$ is the degree of weld cut-back (default value = $20^\circ$)).
A1.5 Lloyd’s Register K joint equations

Notes: When $a<12$ the basic saddle SCF equation should be multiplied by the appropriate short chord correction factor $F1, F2$ etc.

Apply the modified $\beta$ value when predicting SCFs at the SADDLE on $\beta = 1$ joints under axial load or OPB.

The expressions should be calculated using the geometry associated with brace $A$, where brace $A$ is always defined as the brace under consideration.

<table>
<thead>
<tr>
<th>%</th>
<th>Std</th>
<th>Devn</th>
</tr>
</thead>
</table>

**Single axial load**

- $SCF_{CS} = T_{1A} S_{1AB} x (F_{1A} \text{ or } F_{2A})$
- $SCF_{CC} = T_{2A} S_{2AB} + B_{0A} x B_{1A}$
- $SCF_{BS} = T_{3A} S_{1AB} x (F_{1A} \text{ or } F_{2A})$
- $SCF_{BC} = T_{4A} S_{2AB}$

- $\sigma = 18\%$
- $\sigma = 13\%$
- $\sigma = 20\%$
- $\sigma = 23\%$

**Balanced axial load**

- $SCF_{CS} = (T_{1A} S_{1AB} - T_{1B} S_{1BA} IF_{1AB}) x (F_{1A} \text{ or } F_{2A})$
- $SCF_{CC} = (T_{2A} S_{2AB} - T_{2B} S_{2BA} IF_{2AB}) + B_{0A} x B_{1A}$
- $SCF_{BS} = (T_{3A} S_{1AB} - T_{3B} S_{1BA} IF_{3AB}) x (F_{1A} \text{ or } F_{2A})$
- $SCF_{BC} = T_{4A} S_{2AB} - T_{4B} S_{2BA} IF_{4AB}$

- $\sigma = 22\%$
- $\sigma = 25\%$
- $\sigma = 12\%$
- $\sigma = 26\%$

**Single out-of-plane bending**

- $SCF_{CS} = T_{5A} S_{1AB} x (F_{3A})$
- $SCF_{BS} = T_{6A} S_{1AB} x (F_{3A})$

- $\sigma = 17\%$
- $\sigma = 18\%$

**Unbalanced out-of-plane bending**

- $SCF_{CS} = (T_{5A} S_{1AB} + T_{5B} S_{1BA} IF_{5AB}) x (F_{3A})$
- $SCF_{BS} = (T_{6A} S_{1AB} + T_{6B} S_{1BA} IF_{6AB}) x (F_{3A})$

- $\sigma = 14\%$
- $\sigma = 21\%$

**Single in-plane bending**

- $SCF_{C} = T_{7A}$  
- $SCF_{B} = T_{8A}$

- $\sigma = 15\%$
- $\sigma = 17\%$
Balanced in-plane bending

\[ \text{SCF}_{C} = T_{7A} + T_{7B} IF_{7AB} \quad \sigma = 15\% \]
\[ \text{SCF}_{B} = T_{8A} + T_{8B} IF_{8AB} \quad \sigma = 16\% \]

Validity range

The above equations are generally valid for joint parameters within the following limits:

- \[ 0.13 \leq \beta \leq 1.0 \]
- \[ 10 \leq \gamma \leq 35 \]
- \[ 0.25 \leq \tau \leq 1.0 \]
- \[ 30^\circ \leq \theta \leq 90^\circ \]
- \[ 4 \leq \alpha \]
- \[ 0 \leq \zeta \leq 1.0 \]

Note: \( \beta = 1 \) joints at the saddle: \( \beta = 1 - \left( \frac{\zeta}{\gamma} \times \sin^{0.65} (\Psi^\circ) \right) \)

(where \( \Psi^\circ \) is the degree of weld cut-back (default value = 20°)).
A1.6 Lloyd’s Register KT joint equations

Notes: When $a<12$ the basic saddle SCF equation should be multiplied by the appropriate short chord correction factor $F_1, F_2$ etc.

Apply the modified $\beta$ value when predicting SCFs at the SADDLE on $\beta = 1$ joints under axial load or OPB.

The expressions should be calculated using the geometry associated with brace $A$, where brace $A$ is always defined as the brace under consideration, except where otherwise stated.

<table>
<thead>
<tr>
<th>%</th>
<th>Std</th>
<th>Devn</th>
</tr>
</thead>
</table>

### Single axial load

| SCF $CS$ | $T_1A\ S_{1\ AB}\ S_{1\ AC} \times (F_{1A} \text{ or } F_{2A})$ | $\sigma=18\%$ |
| SCF $CC$ | $T_2A\ S_{2\ AB} + B_0A \times B_1A$ (Outer Brace $A$) | $\sigma=13\%$ |
| SCF $BS$ | $T_3A\ S_{1\ AB}\ S_{1\ AC} \times (F_{1A} \text{ or } F_{2A})$ | $\sigma=20\%$ |
| SCF $BC$ | $T_4A\ S_{2\ AB}$ (Outer Brace $A$) | $\sigma=23\%$ |
| SCF $BS$ | $T_3A\ S_{1\ AB}\ S_{1\ AC} \times (F_{1A} \text{ or } F_{2A})$ | $\sigma=23\%$ |

### Balanced axial load (only outer braces A and C loaded)

**Central brace (brace B)**

| SCF $CS$ | $\text{MAX } [(T_{1B}\ S_{1\ BA}\ S_{1\ BC} - T_{1A}\ S_{1\ AB}\ S_{1\ AC}\ IF_{1BA}), (T_{1B}\ S_{1\ BC}\ S_{1\ BA} - T_{1C}\ S_{1\ CB}\ S_{1\ CA}\ IF_{1BC})] \times (F_{1B} \text{ or } F_{2B})$ | $\sigma=22\%$ |
| SCF $CC$ | $\text{MAX } [(T_{2B}\ S_{2\ AB} - T_{2A}\ S_{2\ AC}, IF_{2BA}) + B_0B \times B_1B, (T_{2B}\ S_{2\ BC} - T_{2C}\ S_{2\ BC}) + B_0B \times B_1B]$ | $\sigma=25\%$ |
| SCF $BS$ | $\text{MAX } [(T_{3B}\ S_{1\ BA}\ S_{1\ BC} - T_{3A}\ S_{1\ AB}\ S_{1\ AC}\ IF_{3BA}), T_{3B}\ S_{1\ BC}\ S_{1\ BA} - T_{3C}\ S_{1\ CB}\ S_{1\ CA}\ IF_{3BC}] \times (F_{1B} \text{ or } F_{2B})$ | $\sigma=12\%$ |
| SCF $BC$ | $\text{MAX } [(T_{4B}\ S_{2\ AB} - T_{4A}\ S_{2\ AC}, IF_{4BA}), (T_{4B}\ S_{2\ BC} - T_{4C}\ S_{2\ BC}) \times (F_{1B} \text{ or } F_{2B})]$ | $\sigma=26\%$ |

Where $S_2B = \text{Max} (S_{2\ BA}, S_{2\ BC})$, $S_2A = \text{Max} (S_{2\ AB}, S_{2\ AC})$ and $S_2C = \text{Max} (S_{2\ CB}, S_{2\ CA})$

**Outer brace (brace A)**

| SCF $CS$ | $(T_{1A}\ S_{1\ AB}\ S_{1\ AC} - T_{1C}\ S_{1\ CB}\ S_{1\ CA}\ IF_{1AC}) \times (F_{1A} \text{ or } F_{2A})$ | $\sigma=22\%$ |
SCF_{CC} = (T_2 A S_2 A - T_2 c S_2 c I F_2 A c) + B_0 A x B_1 A \quad \sigma = 25\% \\
SCF_{BS} = (T_3 A S_1 A S_1 A c - T_3 c S_1 c B S_1 c A I F_3 A c) x (F_1 A or F_2 A) \quad \sigma = 12\% \\
SCF_{BC} = (T_4 A S_2 A - T_4 c S_2 c I F_4 A c) \quad \sigma = 26\%

**Single out-of-plane bending**

SCF_{CS} = T_{5 A} S_1 A S_1 A c x (F_3 A) \quad \sigma = 17\% \\
SCF_{BS} = T_{6 A} S_1 A S_1 A c x (F_3 A) \quad \sigma = 18\%

**Unbalanced out-of-plane bending (all braces loaded)**

SCF_{CS} = (T_{5 A} S_1 A S_1 A c + T_{5 B} S_1 B S_1 B c I F_5 A B + T_{5 C} S_1 c B S_1 c A c I F_5 A c) x (F_3 A) \quad \sigma = 14\% \\
SCF_{BS} = (T_{6 A} S_1 A S_1 A c + T_{6 B} S_1 B S_1 B c I F_6 A B + T_{6 C} S_1 c B S_1 c A c I F_6 A c) x (F_3 A) \quad \sigma = 21\%

**Single in-plane bending**

SCF_{C} = T_7 A \quad \sigma = 15\% \\
SCF_{B} = T_8 A \quad \sigma = 17\%

**Balanced in-plane bending (only outer braces A & C loaded)**

**Central brace (brace B)**

SCF_{CS} = \text{MAX} [(T_{1 B} S_1 B A S_1 B c - T_{1 A} S_1 A B S_1 A c I F_1 B A), (T_{1 B} S_1 B B S_1 B c - T_{1 C} S_1 C B S_1 C A c I F_1 B C)] x (F_1 B or F_2 B) \quad \sigma = 22\% \\
SCF_{CC} = \text{MAX} [(T_{2 B} S_2 B - T_2 A, S_2 A c I F_2 B A) + B_0 B x B_1 B, \quad \sigma = 25\% \\
(T_{2 B} S_2 B c - T_2 c S_2 c c I F_2 B C)] + B_0 B x B_1 B] \\
SCF_{BS} = \text{MAX} [(T_{3 B} S_3 B A S_3 B c - T_{3 A} S_1 A B S_1 A c I F_3 B A), \quad \sigma = 12\% \\
(T_{3 B} S_3 B c S_3 B c - T_{3 C} S_3 C B S_3 C A c I F_3 B C)] x (F_1 B or F_2 B)] \\
SCF_{BS} = \text{MAX} [(T_{4 B} S_4 B - T_4 A, S_2 A c I F_4 B A), (T_{4 B} S_4 B c - T_4 c S_2 c c I F_4 B C)] \quad \sigma = 26\%

Where S_2 B = \text{Max} (S_2 B A, S_2 B C), S_2 A = \text{Max} (S_2 A B, S_2 A C) and S_2 c = \text{Max} (S_2 c B, S_2 c A)

**Outer brace (brace A)**

SCF_{CS} = (T_{1 A} S_1 A B - T_{1 C} S_1 C B S_1 C A c I F_1 A c) x (F_1 A or F_2 A) \quad \sigma = 22\% \\
SCF_{CC} = (T_{2 A} S_1 A B - T_{2 C} S_2 C B I F_2 A c) + B_0 A x B_1 A \quad \sigma = 25\% \\
SCF_{BS} = (T_{3 A} S_1 A B - T_{3 C} S_1 C B S_1 C A c I F_3 A c) x (F_1 A or F_2 A) \quad \sigma = 12\% \\
SCF_{BC} = (T_{4 A} S_2 A - T_4 c S_2 c c I F_4 A c) \quad \sigma = 26\%
Single out-of-plane bending

$$SCF_{CS} = T_{5A} S_{1AB} S_{1AC} \times (F_{3A})$$ \quad \sigma=17\%$$

$$SCF_{BS} = T_{6A} S_{1AB} S_{1AC} \times (F_{3A})$$ \quad \sigma=18\%$$

Unbalanced out-of-plane bending (all braces loaded)

$$SCF_{CS} = (T_{5A} S_{1AB} S_{1AC} + T_{5B} S_{1BA} S_{1BC} IF_{5AB}$$

$$+ T_{5C} S_{1CB} S_{1CA} IF_{5AC}) \times (F_{3A})$$ \quad \sigma=14\%$$

$$SCF_{BS} = (T_{6A} S_{1AB} S_{1AC} + T_{6B} S_{1BA} S_{1BC} IF_{6AB}$$

$$+ T_{6C} S_{1CB} S_{1CA} IF_{6AC}) \times (F_{3A})$$ \quad \sigma=21\%$$

Single in-plane bending

$$SCF_C = T_{7A}$$ \quad \sigma=15\%$$

$$SCF_B = T_{8A}$$ \quad \sigma=17\%$$

Balanced in-plane bending (only outer braces A & C loaded)

Central brace (brace B)

$$SCF_C = \text{MAX} [T_{7B} + T_{7A} IF_{7BA}, (T_{7B} + T_{7C} IF_{7BC})]$$ \quad \sigma=15\%$$

$$SCF_B = \text{MAX} [T_{8B} + T_{8A} IF_{8BA}, (T_{8B} + T_{8C} IF_{8BC})]$$ \quad \sigma=16\%$$

Outer brace (brace A)

$$SCF_C = T_{7A} + T_{7C} IF_{7AC}$$ \quad \sigma=15\%$$

$$SCF_B = T_{8A} + T_{8C} IF_{8AC}$$ \quad \sigma=16\%$$

Validity range

The above equations are generally valid for joint parameters within the following limits:

$$0.13 \leq \beta \leq 1.0$$

$$10 \leq \Psi \leq 35$$

$$0.25 \leq \tau \leq 1.0$$

$$30^\circ \leq \theta \leq 90^\circ$$

$$4 \leq \alpha$$

$$0 \leq \zeta \leq 1.0$$

Note: \( \beta = 1 \) joints at the saddle: \( \beta = 1 - \left( \frac{1}{7} \times \sin^{0.65}(\Psi^\circ) \right) \)

(where \( \Psi^\circ \) is the degree of weld cut-back (default value = 20°))
A1.7 Parametric Equations Expressions

A1.7.1 T Factors - T joint factors

Note: Apply the modified $\beta$ value when predicting SCFs at the SADDLE on $\beta = 1$ joints under axial load or OPB

Axial load

\[ T_1 = \tau \gamma^2 \beta \left(2.12 - 2\beta\right) \sin^2 \theta \]
\[ T_2 = \tau \gamma^2 \left(3.5 - 2.4\beta\right) \sin^{0.3} \theta \]
\[ T_3 = 1 + \tau^{0.6} \gamma^{1.3} \beta \left(0.76 - 0.7\beta\right) \sin^{2.2} \Theta \]
\[ T_4 = 2.6\beta^{0.65} \gamma^{(0.3 - 0.5\beta)} \]

Out-of-plane bending

\[ T_5 = \tau \gamma \beta \left(1.4 - \beta^3\right) \sin^{1.7} \Theta \]
\[ T_6 = 1 + \tau^{0.6} \gamma^{1.3} \beta \left(0.27 - 0.2\beta^5\right) \sin^{1.7} \Theta \]

In-plane bending

\[ T_7 = 1.22\tau^{0.8} \beta \gamma^{(1 - 0.68\beta)} \sin^{(1 - \beta)} \Theta \]
\[ T_8 = 1 + \tau^{0.2} \gamma \beta \left(0.26 - 0.21\beta\right) \sin^{1.5} \Theta \]

A1.7.2 X Factors = X joint factors

Note: Apply the modified $\beta$ value when predicting SCFs at the SADDLE on $\beta = 1$ joints under axial load or OPB

Axial load

\[ X_1 = \tau \beta \gamma^{1.3} \left(1.46 - 1.4\beta^7\right) \sin^2 \Theta \]
\[ X_2 = (0.36 + 1.9 \tau \gamma^{0.5} \exp\left(\beta^{1.5} \gamma^{0.5}\right)) \left(\sin \Theta + 3\cos \Theta\right) \]
\[ X_3 = 1 + 0.6 \times X_1 \]
\[ X_4 = (1.3 + 0.06 \tau \gamma \exp\left(\beta^{2} \gamma^{0.5}\right)) \sin^{-1} \Theta \]

Out-of-plane bending

\[ X_5 = \tau \beta \gamma^{1.3} \left(0.63 - 0.6\beta^3\right) \sin^2 \Theta \]
\[ X_6 = 1 + \tau \beta \gamma^{1.5} \left(0.19 - 0.185\beta^3\right) \sin^{7(1 - \beta^2)} \Theta \]

In-plane bending
X7 = τ^{0.8} \beta \gamma^{0.3\beta-0.5} (1-0.32\beta^3) \sin^{0.5} \Theta

X8 = 1 + τ^{0.8} \beta \gamma (0.32-0.25\beta) \sin^{1.5} \Theta

A1.7.3 S Factors - The stiffening effect of an additional brace

Note: Apply the modified \beta value when predicting SCFs at the SADDLE on \beta = 1 joints under axial load or OPB (ie Eqn S1) on \beta = 1

\[ S_{1,j} = \left[ 1 - 0.4x \exp\left(-30x^2 \frac{b_i}{b_j} x^2 \frac{\sin \theta_i}{\sin \theta_j} \right) \right] \]

\[ S_{2,j} = \left[ 1 + \exp\left(2x^2 \frac{\sin \Theta_i}{\sin \Theta_j} x \gamma^{(0.5)} \right) \right] \]

Where \( x_j = 1 + \frac{\zeta_j \sin \theta_j}{\beta_j} \)

\( \zeta_{ij} \) = Gap between weld toes of brace I and brace j/chord diameter

A1.7.4 IF Factors - Influence functions for K and KT joint expressions

Note: Apply the modified \beta value when predicting SCFs at the SADDLE on \beta = 1 joints under axial load or OPB

Axial load

\[ \text{IF}_{1,j} = \beta_i (2.13-2\beta_i) \gamma^{0.2} \sin \Theta_i \left( \frac{\sin \theta_i}{\sin \theta_j} \right)^P \exp (-0.3x_{ij}) \text{ where } P = 1 \text{ if } \Theta_i > \Theta_j \]

\[ \text{IF}_{2,j} = [20-8(\beta_i+1)^2] \exp (-3x_{ij}) \]

\[ \text{IF}_{3,j} = \beta_i (2-1.8\beta_i) \gamma^{0.2} \left( \frac{\beta_{ij}}{\beta_{ji}} \right) \left( \frac{\sin \theta_i}{\sin \theta_j} \right)^P \exp (-0.5x_{ij}) \text{ where } P = 2 \text{ if } \Theta_i > \Theta_j \]

Out-of-plane bending

\[ \text{IF}_{5,j} = 0.6\gamma \left( \frac{\sin \theta_i}{\sin \theta_j} \right) \exp (-3x_{ij}) \]

\[ \text{IF}_{6,j} = 0.14\beta_i \gamma 1.5 \left( \frac{\sin \theta_i}{\sin \theta_j} \right) \exp (-3x_{ij}) \]

In-plane bending

\[ \text{IF}_{7,j} = 1.5 \tau_i^{(-2)} \exp(-3x_{ij}) \]

\[ \text{IF}_{8,j} = [40(\beta_i - 0.75)^2 - 2.5] \exp(-3x_{ij}) \]

Where \( x_j = 1 + \frac{\zeta_j \sin \theta_j}{\beta_j} \)
ζ₀ = Gap between weld toes of brace I and brace j/chord diameter

A1.7.5 B Factors - Approximation of the chord in-plane bending

\[ B_0 = \frac{Ct(B - τ/2)(α/2 - β/\sin θ) \sin θ}{(1 - 3/(2γ))} \quad \text{for single axial load} \]

\[ B_0 = 0.00 \quad \text{for balanced axial load} \]

\[ B_1 = 1.05 + \frac{30 \cdot α^{1.5}(1.2 - β)(\cos^4 θ + 0.15)}{β} \]

Chord-end fixity parameter (C) \( 0.5 \leq C \leq 1.0 \)

C = 0.5 for fully fixed chord ends

C = 1.0 for pinned chord ends

For a structural analysis a value of C = 0.7 is normally assumed.

A1.7.6 F Factors - Short chord correction factors

Note: Apply the modified \( β \) value when predicting SCFs on \( β = 1 \) joints

\[ F_1 = \begin{cases} 1 - (0.83β - 0.56β^2 - 0.02) γ^{0.23} \exp(-0.21γ^{0.16} α^{2.5}) & \alpha < 12 \\ 1.0 & \alpha \geq 12 \end{cases} \]

\[ F_2 = \begin{cases} 1 - (1.43β - 0.97β^2 - 0.03) γ^{0.04} \exp(-0.71γ^{0.38} α^{2.5}) & \alpha < 12 \\ 1.0 & \alpha \geq 12 \end{cases} \]

\[ F_3 = \begin{cases} 1 - (0.55β^{1.8} γ^{0.16} \exp(-0.49γ^{0.89} α^{1.8}) & \alpha < 12 \\ 1.0 & \alpha \geq 12 \end{cases} \]
A2. EFTHYMIOU PARAMETRIC EQUATIONS

A2.1 Efthymiou Equations for T/U Joints

Note: When $\alpha < 12$ the basic saddle SCF equation should be multiplied by the appropriate short chord correction factor $F_1$, $F_2$ etc.

**Axial load**

$$SCF_{CS} = \gamma^{0.1} \tau^{1.1} \{ 1.11 - 3(\beta - 0.52)^2 \} \sin^{1.6} \Theta + (2C - 1)(0.8\alpha - 6) \tau \beta^2(1 - \beta^2)^{0.5} \sin^2(2\Theta)$$

$$SCF_{CC} = \gamma^{0.2} \tau \{ 2.65 + 5(\beta - 0.65)^2 \} + \tau \beta(0.5C \alpha - 3) \sin \Theta$$

$$SCF_{BS} = 1.3 + \gamma \tau^{0.52} \alpha^{0.1} \{ 0.187 - 1.25(\beta - 0.96) \} \sin^{2.7 - 0.010} \Theta$$

**Out-of-plane bending**

$$SCF_{BS} = 1.3 + \gamma \tau^{0.2} \{ 0.12 \exp(-4\beta) + 0.011\beta^2 - 0.045 \} + \tau \beta (0.2C \alpha - 1.2)$$

**In-plane bending**

$$SCF_{CC} = 1.45 \beta^{0.85} \gamma^{1.068} \sin^{0.7} \Theta$$

$$SCF_{BC} = 1 + 0.65 \beta^{0.85} \gamma^{1.09 - 0.77} \sin^{0.069 - 1.16} \Theta$$

**Chord-end fixity parameter** ($C$) $0.5 \leq C \leq 1.0$

$C = 0.5$ for fully fixed chord ends
$C = 1.0$ for pinned chord ends
For a structural analysis a value of $C = 0.7$ is normally assumed.

**Short cord correction factors** ($\alpha < 12$)

$$F_1 = 1 - (0.83\beta - 0.56\beta^2 - 0.02)\gamma^{0.23} \exp(-0.21\gamma^{0.16} \alpha^{2.5})$$
$$F_2 = 1 - (1.43\beta - 0.97\beta^2 - 0.03)\gamma^{0.04} \exp(-0.71\gamma^{1.30} \alpha^{2.5})$$
$$F_3 = 1 - 0.55\beta^{0.85} \gamma^{0.16} \exp(-0.49\gamma^{0.89} \alpha^{1.8})$$

Under axial load, apply expression $F_1$ if $C < 0.7$ and apply expression $F_2$ if $C \geq 0.7$.
Validity range

The above equations for T/Y joints are generally valid for joint parameters within the following limits:

\[ 0.2 \leq \beta \leq 1.0 \]
\[ 8 \leq \gamma \leq 32 \]
\[ 0.2 \leq \tau \leq 1.0 \]
\[ 20^\circ \leq \Theta \leq 90^\circ \]
\[ 4 \leq \alpha \leq 40 \]
A2.2 Efthymiou Equations for X Joints

Note: When \( \alpha < 12 \) the basic saddle SCF equation should be multiplied by the appropriate short chord correction factor \( F_1, F_2 \) etc.

Under axial load, apply expression \( F_1 \) if \( C < 0.7 \) and apply expression \( F_2 \) if \( C > 0.7 \)

\[
\begin{align*}
F_1 &= 1 - (0.83 \beta - 0.56 \beta^2 - 0.02) \gamma^{0.23} \exp(-0.21 \gamma^{1.16} \alpha^{2.5}) \\
F_2 &= 1 - (1.43 \beta - 0.97 \beta^2 - 0.03) \gamma^{0.04} \exp(-0.71 \gamma^{1.38} \alpha^{2.5}) \\
F_3 &= 1 - 0.55 \beta^{1.8} \gamma^{0.16} \exp(-0.49 \gamma^{0.89} \alpha^{1.3})
\end{align*}
\]

Under axial load, apply expression \( F_1 \) if \( C < 0.7 \) and apply expression \( F_2 \) if \( C \geq 0.7 \)

Validity range

The above equations for X joints are generally valid for joint parameters within the following limits:

- \( 0.2 \leq \beta \leq 1.0 \)
- \( 8 \leq \gamma \leq 32 \)
- \( 0.2 \leq \tau \leq 1.0 \)
- \( 20^\circ \leq \Theta \leq 90^\circ \)
- \( 4 \leq \alpha \leq 40 \)
A2.3 Efthymiou Equations for K Joints

Note: When $\alpha < 12$ the basic saddle SCF equation should be multiplied by the appropriate short chord correction factor $F_1$, $F_2$ etc

The expressions should be calculated using the geometry associated with brace $A$, where brace $A$ is always defined as the brace under consideration.

For $K$ joint equations, the gap parameter, $x = 1 + z \sin \theta_A/\beta_A$

**Single axial load**

- Short Cord Factor

**SCF$_{CS}$**

$$SCF_{CS} = \gamma \tau_A^{1.1} \left\{ 1.11 - 3(\beta_A - 0.52)^2 \right\} \sin^{1.6} \Theta + (2C - 1)(0.8\alpha - 6) \tau_A \beta^2_A(1 - \beta_A^2)^{0.5} \sin^2 (2\Theta_A)$$

**SCF$_{CC}$**

$$SCF_{CC} = \gamma \tau_A \left[ 2.65 + 5(\beta_A - 0.65)^2 \right] + \tau_A \beta_A (0.5C \alpha - 3) \sin \Theta_A$$

**SCF$_{BS}$**

$$SCF_{BS} = 1.3 + \gamma \tau_A^{0.52} \alpha^{0.1} \left\{ 0.187 - 1.25 \beta_A 1.17 \beta_A - 0.96 \right\} \sin^{(2.7-0.01\alpha)} \Theta_A$$

**SCF$_{BC}$**

$$SCF_{BC} = 3 + \gamma \tau_A^{0.2} \left\{ 0.12 \exp(-4\beta_A) + 0.011 \beta_A^2 - 0.045 \right\} + \tau_A \beta_A (0.2C \alpha - 1.2)$$

**Balanced axial load**

**SCF$_{CC}$**

$$SCF_{CC} = \gamma \left[ 2.65 + 5(\beta_A - 0.65)^2 \right] + \tau_A \beta_A (0.5C \alpha - 3) \sin \Theta_A$$

**SCF$_{BS}$**

$$SCF_{BS} = 1.3 + \gamma \tau_A \left\{ 0.187 - 1.25 \beta_A \right\} \sin^{(2.7-0.01\alpha)} \Theta_A$$

**Single out-of-plane bending**

**SCF$_{CS}$**

$$SCF_{CS} = \left[ \gamma \tau_A \beta_A (1.7 - 1.05\beta_A^3) \sin^{1.6} \Theta_A \right] [1 - 0.08(\beta_A - 0.8) \exp(-0.8x)]$$

**SCF$_{BS}$**

$$SCF_{BS} = \tau_A (0.54) \gamma (0.05) (0.99 - 0.47\beta_A + 0.08\beta_A^3) SCF_{CS}$$

**Unbalanced out-of-plan bending**

**SCF$_{CS}$**

$$SCF_{CS} = \left[ \gamma \tau_A \beta_A (1.7 - 1.05\beta_A^3) \sin^{1.6} \Theta_A \right] [1 - 0.08(\beta_A - 0.8) \exp(-0.8x)] + \left[ \gamma \beta_A \beta_b (1.7 - 1.05\beta_A^3 \sin^{1.6} \Theta_b \right] [1 - 0.08(\beta_A - 0.8) \exp(-0.8x)] x \left[ 2.05 \beta_{max} \exp(-1.3x) \right]$$

**SCF$_{BS}$**

$$SCF_{BS} = \tau_A (0.54) \gamma (0.05) (0.99 - 0.47\beta_A + 0.08\beta_A^3) \left\{ SCF_{CS} \right\}$$
Single in-plane bending

$$\text{SCF}_{\text{CC}} = 1.45 \beta_{\Lambda} \tau_{\Lambda}^{0.85} \gamma^{1-0.68\beta} \sin^{0.7} \Theta_{\Lambda}$$

$$\text{SCF}_{\text{BC}} = 1 + 0.65 \beta_{\Lambda} \tau_{\Lambda}^{0.4} \gamma^{1.09-0.77\beta} \sin^{(0.06\gamma-1.16)} \Theta_{\Lambda}$$

Balanced in-plane bending

$$\text{SCF}_{\text{CC}} = [1.45\beta_{\Lambda} \tau_{\Lambda}^{0.85} \gamma^{1-0.68\beta} \sin^{0.7}\Theta_{\Lambda}][1 + 0.46 \beta_{\Lambda}^{1.2} \exp(-3\zeta)]$$

$$\text{SCF}_{\text{BC}} = 1 + 0.65\beta_{\Lambda} \tau_{\Lambda}^{0.4} \gamma^{1.09-0.77\beta} \sin^{(0.06\gamma-1.16)} \Theta_{\Lambda}$$

Chord-end fixity parameter (C) \(0.5 \leq C \leq 1.0\)

C = 0.5 for fully fixed chord ends
C = 1.0 for pinned chord ends
For a structural analysis a value of C = 0.7 is normally assumed.

Short cord correction factors (\(\alpha < 12\))

$$F_1 = 1 - (0.83\beta - 0.56\beta^2 - 0.02)\gamma^{0.25} \exp(-0.21\gamma^{1.16}) \alpha^{2.5})$$

$$F_2 = 1 - (1.43\beta - 0.97\beta^2 - 0.03)\gamma^{0.04} \exp(-0.71\gamma^{1.30}) \alpha^{2.5})$$

$$F_3 = 1 - 0.55\beta^{1.8}\gamma^{0.16} \exp(-0.49\gamma^{1.89}) \alpha^{1.3})$$

$$F_4 = 1 - 107\beta^{1.88}\exp(0.16\gamma^{1.66}) \alpha^{2.4})$$

Under axial load, apply expression F1 if \(C < 0.7\) and apply expression F2 if \(C \geq 0.7\)

Validity range

The above equations for X joints are generally valid for joint parameters within the following limits:

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<tr>
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<th>Maximum</th>
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</thead>
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</tr>
<tr>
<td>(\gamma)</td>
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<td>32</td>
</tr>
<tr>
<td>(\tau)</td>
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<td>1.0</td>
</tr>
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<td>(\Theta)</td>
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<td>90(^{\circ})</td>
</tr>
<tr>
<td>(\alpha)</td>
<td>4</td>
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<tr>
<td>(\zeta)</td>
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</table>
A2.4 Efthymiou Equations for KT Joints

Note: The expressions should be calculated using the geometry associated with the brace under consideration i.e. Central Brace B or Outer Brace A.

Balanced axial load
Central brace (brace B)

\[
SCF_C = \tau_B^{0.9} \gamma^{0.5} (0.67 - \beta_B^3 + 1.16\beta_B) \sin \Theta_B \left[ \frac{\sin \theta_{max}}{\sin \theta_{min}} \right]^{0.3} \left[ \frac{\beta_{max}}{\beta_{min}} \right]^{0.3} \times [1.64 + 0.29 \beta_B^{(-0.38)} \text{ATAN}(8x_{\text{MAX}}(\zeta_{AB}, \zeta_{BC}))]
\]

\[
SCF_B = 1 + [SCF_C](1.97 - 1.57\beta_B^{0.25}) \tau_B^{(-0.14)} \sin^{0.7}\Theta_B \quad \text{(ATAN in radians)}
\]

Outer brace (brace A)

\[
SCF_C = \tau_A^{0.9} \gamma^{0.5} (0.67 - \beta_A^3 + 1.16\beta_A) \sin \Theta_A \left[ \frac{\sin \theta_{max}}{\sin \theta_{min}} \right]^{0.3} \left[ \frac{\beta_{max}}{\beta_{min}} \right]^{0.3} \times [1.64 + 0.29 \beta_B^{(-0.38)} \text{ATAN}(8\zeta_{AC})]
\]

\[
SCF_B = 1 + [SCF_C](1.97 - 1.57\beta_A^{0.25}) \tau_A^{(-0.14)} \sin^{0.7}\Theta_A
\]

Where \( \zeta_{AC} = \zeta_{AB} + \zeta_{BC} + \frac{\beta_B}{\sin \theta_B} \) \quad \text{(ATAN in radians)}

Unbalanced out-of-plane bending
Central brace (brace B)

\[
SCF_C = \left[ \gamma \tau_B \beta_B (1.87 - 1.05\beta_B^3) \sin^{1.6}\Theta_B \right] [1 - 0.08(\beta_A \gamma)^{0.5} \exp(-0.8x_{AB})]^{(\theta_A / \beta_B)^2}
\]

\[
[1 - 0.08(\beta_C \gamma)^{0.5} \exp(-0.8x_{BC})]^{(\theta_C / \beta_B)^2}
\]

\[
+ [\gamma \tau_B \beta_B (1.7 - 1.05\beta_B^3) \sin^{1.6}\Theta_B]
\]

\[
[1 - 0.08(\beta_A \gamma)^{0.5} \exp(-0.8x_{AB})] [2.05 \beta_B^{0.5} \exp(-1.3x_{AB})]
\]

\[
+ [\gamma \tau_B \beta_B (1.7 - 1.05\beta_B^3) \sin^{1.6}\Theta_B]
\]

\[
[1 - 0.08(\beta_C \gamma)^{0.5} \exp(-0.8x_{BC})] [2.05 \beta_B^{0.5} \exp(-1.3x_{BC})]
\]

Where \( x_{AB} = 1 + \frac{\zeta_{AB} \sin \theta_B}{\beta_B} \) and \( x_{BC} = 1 + \frac{\zeta_{BC} \sin \theta_B}{\beta_B} \)

\[
SCF_B = \tau_B^{(-0.54)} \gamma^{(-0.05)} (0.99 - 0.47\beta_B + 0.08 \beta_B^{4/5}) SCF_C
\]

Outer brace (brace A)

\[
SCF_C = \left[ \gamma \tau_B \beta_B (1.7 - 1.05\beta_B^3) \sin^{1.6}\Theta_B \right]
\]

\[
x [1 - 0.08(\beta_B \gamma)^{0.5} \exp(-0.8x_{AB})] [1.0 - 0.08(\beta_C \gamma)^{0.5} \exp(-0.8x_{AC})]
\]

\[
+ [\gamma \tau_B \beta_B (1.7 - 1.05\beta_B^3) \sin^{1.6}\Theta_B]
\]
\[
[1-0.08(\beta A\gamma)^{0.5}\exp(-0.8x_{AB})][2.05\beta_{\text{max}}^{0.5}\exp(-1.3x_{AB})]
\]
\[\quad+ \left[\gamma C\beta C(1.7-1.05\beta C)^{1.6}\right] \]
\[\quad+ [1-0.08(\beta A\gamma)^{0.5}\exp(-0.8x_{AC})][2.05\beta_{\text{max}}^{0.5}\exp(-1.3x_{AC})]\]
\]

Where \(x_{\text{AB}} = 1 + \frac{\zeta A \sin \Theta A}{\beta A}\) and \(x_{\text{AC}} = 1 + \frac{(\zeta A + \zeta B + \beta A / \sin \Theta A) \sin \Theta A}{\beta A}\)

SCF_B = \(\tau A^{(-0.54)} \gamma^{(-0.05)} (0.99-0.47\beta A + 0.08 \beta A^2)\) SCF_C

**Balanced in-plane bending**

**Central brace (brace B)**

SCF_C = 1.45 \(\beta B \tau B^{0.85} \gamma^{(1.068\beta B)} \sin^{0.7} \Theta A [1+0.46\beta B^{1.2} \exp(-3\text{Min}(\zeta AB, \zeta BC))]\]

SCF_B = 1 + 0.65\(\beta B \tau B^{0.4} \gamma^{(1.09-0.77\beta B)} \sin^{0.06}\Theta A \]

**Outer brace (brace A)**

SCF_C = 1.45\(\beta A \tau A^{0.85} \gamma^{(1.068\beta A)} \sin^{0.7}\Theta A [1+0.46\beta A^{1.2} \exp(-3\zeta AB)]\]

SCF_B = 1 + 0.65\(\beta A \tau A^{0.4} \gamma^{(1.09-0.77\beta A)} \sin^{0.06}\Theta A \]

**Chord-end fixity parameter (C)** \(0.5 < C < 1.0\)

C = 0.5 for fully fixed chord ends
C = 1.0 for pinned chord ends

For a structural analysis a value of C = 0.7 is normally assumed.

**Validity range**

The above equations for KT joints are generally valid for joint parameters within the following limits:

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APPENDIX B

SIMPLE JOINT SCH ASSESSMENT DATABASE
INDEX

Tables B1.1 - B1.15  T/Y Joint Geometries and SCFs
Tables B2.1 - B2.5  X Joint Geometries and SCFs
Tables B3.1 - B3.6  K Joint Geometries and SCFs

APPENDIX B REFERENCES
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List of TY Joint Geometries and SCFs.

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Table B.15
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List of Joint Geometries and SCFs

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<th>Unbalanced OPB</th>
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List of K Joint Geometries and SCFs

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**Table B3.6**

List of K Joint Geometries and SCFs
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1. UKOSRP I final report
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APPENDIX C - EXTRAPOLATION PROCEDURES

The extrapolation methods used by Lloyd’s Register in conjunction with acrylic modelling to determine the brace to chord intersection SCFs at various positions are described below:

METHOD 1

A linear extrapolation of the SCFs corresponding to the principal stresses at the two rosettes nearest the junction. At the rosette nearest the intersection the largest of the numerical principal stress SCFs is used and at the second rosette the SCF which is algebraically nearest to it. Thus, with SCFs of -3 and 1 at the nearest rosette and -1 and 2 at the other one, the extrapolation would be carried out through -3 and -1.

METHOD 2

As for Method 1 except that a non-linear extrapolation is used on a quadratic curve through the three measuring points.

METHOD 3

A linear extrapolation is carried out of the SNCFs based on the strains normal to the intersection at the two points nearest to the intersection. This is then converted to an SCF using \( \text{UNCF}_{90} \) (ie the SNCF corresponding to the strain parallel to the intersection at the measuring point nearest to the intersection).

\[
\text{SCF} = \frac{\text{SNCF} + (\nu \times \text{SNCF}_{90})}{1 - \nu^2}
\]

Where \( \nu \) = Poisson’s Ratio

METHOD 4

As for Method 3 but using a non-linear extrapolation on a quadratic curve through the three measuring points.

METHOD 5

Data given in this method is either the SNCF at an isolated single element gauge or the SCF corresponding to the greatest principal stress at an isolated rosette.
The methods of extrapolation which can be employed at the various gauge configurations at each position around the junction are therefore as follows:

(a) Single isolated rosette or single element gauge

(b) Two single element gauges aligned normal to the intersection with one orthogonal gauge

(c) Three single element gauges aligned normal to the intersection with one orthogonal gauge

(d) Two 45° rosettes, each with one element aligned normal to the intersection

(e) Three 45° rosettes, each with one element aligned normal to the intersection