LITERATURE SURVEY ON FATIGUE STRENGTHS OF LOAD-CARRYING FILLET WELDED JOINTS FAILING IN THE WELD

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LITERATURE SURVEY ON FATIGUE STRENGTHS OF LOAD-CARRYING FILLET WELDED JOINTS FAILING IN THE WELD

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SUMMARY

In load-carrying fillet welded joints, which may involve welds either transverse or parallel to the direction of the applied stress (or a combination of the two), there are two potential modes of fatigue cracking. One involves failure at the weld toe and the other cracking from the root through the weld throat. However, if weld throat failure occurs it is clear that the optimum strength of the joint has not been developed, since a higher strength could be obtained by increasing the weld size. When toe failure supervenes no further strength increase is possible by increasing the weld size. Hence in the current fatigue design rules (1) two sets of design stresses are given, one relating to plate failure (Class F) and one to weld throat failure (Class W), and the effect of using those design stresses should be to arrive at joint dimensions close to the optimum.

However, there are several reasons why the existing Class W design stresses may be incorrect so the available test data for transverse load carrying fillet welds subjected to tensile loading have been re-analysed.
1. INTRODUCTION

In load-carrying fillet welded joints, which may involve welds either transverse or parallel to the direction of the applied stress (or a combination of the two), there are two potential modes of fatigue cracking. One involves failure at the weld toe and the other cracking from the root through the weld throat. However, if weld throat failure occurs it is clear that the optimum strength of the joint has not been developed, since a higher strength could be obtained by increasing the weld size. Hence in the current fatigue design rules (1a) two sets of design stresses are given, one relating to plate failure (Class F2) and one to weld throat failure (Class W), and the effect of using those design stresses should be to arrive at joint dimensions close to the optimum.

However, there are several reasons why the existing Class W design stresses may be incorrect, including the following:

a) The data for transverse load-carrying fillet welds used in the original analysis were fairly limited, being derived from only seven investigations. There are now many more data available.

b) At the time that the Class W design curve was produced Class F2 did not exist, so that Class W was derived by comparison with Class F. However transverse load-carrying fillet welded joints are now included in Class F2 as far as plate failure is concerned so that the stresses given by Class W are now inconsistent.

c) The data which were used were all obtained using relatively thin specimens (typically 13 mm) made with double fillet welds and subjected to axial loading. Since that time a correction factor to allow for the influence of plate thickness has been introduced but has not been carried through into Class W. There is therefore a need to examine the implications of that amendment.

In view of the above, the available test data for transverse load carrying fillet welds subjected to tensile loading have been re-analysed and the results are set out below.
2. NOMENCLATURE

- $T_{p,t}$: Main plate thickness
- $T_c$: Thickness of central cross plate
- $H$: Weld leg length
- $W$: Assumed total length of half crack at failure ($= H + 0.5 T_p$)
- $K$: Stress intensity factor
- $A_1$): Functions of $\frac{H}{T_p}$ (see equation [1] and Appendix A)
- $A_2$): Instantaneous crack length
- $\alpha$: Non-dimensional crack length $\frac{a}{W}$
- $\sigma_p$: Stress applied to plate
- $\sigma_w$: Stress on weld throat, as defined by equation [18]
- $C,m$: Constants in the Paris crack growth equation (equation [2])
- $D = \frac{1}{c}$
- $U,V$: Particular values of D (equations [10] and [11])
- $N$: Cycles
- $I$: Integral defined by equation [5]
- $\sigma^*$: Generalised stress parameter defined by equation [8]
- $X$: A specific value of $\frac{\sigma_w}{T}$
- $a_1$: Initial crack length (for fillet weld $a_1 = 0.5 T_p$)
- $S$: Fatigue strength of a joint with thickness $t$
- $S_{th}$: Fatigue strength of a joint with thickness $t_{th}$
- $t_{th}$: ‘Basic’ joint thickness
3. FRACTURE MECHANICS ANALYSIS

In general, the problem of specifying the optimum weld size for load carrying transverse fillet welded joints is more complex than making a simple comparison of weld and plate load-carrying areas. The fatigue strengths of joints that fail from the root depend on many variables, chiefly the weld size, plate thickness and depth of weld penetration, the last two defining the size of pre-existing defect at the root. It would be impractical to attempt to specify the fatigue strengths for weld failures in terms of simple S-N curves for all geometric configurations that are likely to occur in practice. Equally, one would expect the available test results, when plotted as an S-N curve on the basis of weld throat stress, to be relatively widely scattered, simply on account of the side variation in the geometrical parameters of the various joints which have been tested. A better approach is to make use of the fact that fatigue cracks are thought to initiate early on in the lives of such joints, and to use a fracture mechanics crack propagation relationship to calculate the fatigue strength. In that way the influence of variations in joint geometry can be eliminated, so that one might reasonably expect the scatter in test results to be reduced.

As far as is known, the only solution for the stress intensity factor (k) for a crack at the root of such a weld is that published by Frank (2). The geometry of the joint, with relevant dimensions, is shown in Fig.1. The analysis below will be limited to joints in which the following conditions are met:

a) The weld angle, \( \theta = 45^\circ \) Frank found that although \( \theta \) influenced the value of \( K \), it was not very significant. Also, in practice, it would usually be necessary to assume an angle, and \( 45^\circ \) is the value normally assumed in design.

b) The main plates and cross plate are of equal thickness, ie \( T_p = T_c \). The effect of varying the ratio \( T_p/T_c \) was not investigated by Frank but it would not be expected to be significant for typical joint dimensions used in practice.

For these conditions the stress intensity factor of a root crack of total length \( 2a \) can be written as

\[
K = \sigma_p \sqrt{\pi a} f \left\{ \frac{a}{W} \cdot \frac{H}{T_p} \right\} \quad [1]
\]

Where \( W, H \) and \( T_p \) are defined in Fig 1 and the function \( f(\cdot) \) is defined in Appendix A.

Now it is well known that, for pulsating tension loading, the rate of propagation of a fatigue crack \( \frac{da}{dN} \) can, to a close approximation, be related to the range of stress intensity factor \( \Delta K \) by the Paris relationship:

\[
\frac{da}{dN} = C (\Delta K)^m \quad [2]
\]

Where \( C \) and \( m \) are material constants. This can be integrated to predict the fatigue life of a crack. Thus, from equations [1] and [2],
\[ \int \frac{a_2}{a_1} \frac{da}{C(a_2^{n} W - N)} = m \frac{m}{2} - 1 \]  

Alternatively, expressing \( a \) in non-dimensional form as \( \frac{a}{W} = \) equation [3] can be rewritten as

\[ \int \frac{a_1}{a_2} \frac{da}{f\left[a, \frac{H}{T_p}, \sqrt{a^2}\right]^m} = C\left(\Delta \sigma_p\right)^n W^{-1}N \]  

Where the crack propagates from an initial size \( \frac{a_1}{W} = \) to a size at failure \( a_2 \), \( = \frac{a_1}{W} \) in N cycles. Hence, assuming failure to occur when \( a_1 = W \), so that \( = 1.0 \), and assuming also that \( m = 3.0 \), which is a typical value for structural steels, equation [4] becomes

\[ 1.0 \int \frac{a}{a_1} \frac{da}{f\left[a, \frac{H}{T_p}, \sqrt{a^2}\right]^3} = C\left(\Delta \sigma_p\right)^3 W^{-1}N \]  

Thus, writing the integral in equation [5] as \( I \), for brevity, we get

\[ I = C\left(\Delta a_p\right)^3 W^{-1}N \]  

Clearly, the value of \( I \) can be evaluated for any particular joint geometry under consideration and some typical values are shown in Fig 2.

It will be seen that equation [6] can be rewritten as

\[ \{ \Delta a_p \left( \frac{\sqrt{W}}{T} \right)^{\frac{1}{2}} \}^3 N = D \]  

Where \( D \equiv \frac{1}{T} \)  

This represents an equation of an S-N curve with ‘stress’ replaced by the generalised stress parameter \( a^* \), where

\[ \Delta a^* = \Delta a_p \left( \frac{\sqrt{W}}{T} \right)^{\frac{1}{2}} \]
4. EXPERIMENTAL RESULTS

Transverse load-carrying fillet welds of the type under consideration have been tested by several investigators and in some instances (2, 4-22) the joint geometries have been such that failure occurred in the weld throat. All the available data have been analysed by plotting the results in terms of $\Delta a^*$ and they are shown in Figs 3-22. These results only include specimens which were tested under tensile loading and in which the welds appeared to have virtually zero penetration. The relevant joint geometries are summarised in Table 1, which also shows the corresponding values of

$$\frac{\Delta a^*}{\Delta a_p} = \left( \frac{VW}{T} \right) \frac{1}{2}$$

For ease of comparison, Figs 3-22 also show the theoretical mean curve deduced from the fracture mechanics analysis assuming $C = 1.83 \times 10^{-13}$. This is the average value $m = 3$, assuming that $\frac{da}{dN}$ is expressed in mm/cycle and $\Delta K$ in N/mm$^{3/2}$ (3). Meanwhile, statistical analysis of the data (504 results), assuming a relationship of the form

$$N = D$$

showed that the standard deviation of $\zeta n(N)$ was 0.7925 and that the mean and mean -2 standard deviations curves could be represented by $D = 6.255 \times 10^{12}$ and $1.272 \times 10^{12}$ respectively. On the same basis the mean of the theoretical scatter band corresponds to

$$D = \frac{1}{1.83 \times 10^{-13}} = 5.46 \times 10^{12}$$

The mean and lower limit experimental curves are also indicated in Figs 3-22.

Thus, the joint behaviour when weld failure occurs can clearly be represented by a curve of the form

$$N = U$$

where $U$ is some selected value of $D$.

Similarly, the strength of joints failing from the weld toe can, from BS 5400, be written as

$$N = V$$

where the value of $V$ depends upon the selected probability of failure.
Hence, in order for failure in the plate and in the weld throat to occur at the same time, equations [10] and [11] must be satisfied simultaneously. Thus, rewriting [10] in terms of $\Delta a_P$, using equation [8], we get

$$ (\Delta a_P)^3 \frac{VW}{T} N = U $$

[12]

so that, from [11] and [12]

$$ \frac{VW}{T} = \frac{U}{V} $$

[13]

There are several possible strategies for selecting the appropriate values of $U$ and $V$. The two most obvious possibilities are:

1. to use values corresponding to the mean -2 standard deviations experimental curve for weld throat failure ($S'N = 1.272 \times 10^{12}$) and the similar curve for Class F_2 in BS 5400, since Class F_2 includes this type of joint and relates to failure from the weld toe ($S'N = 4.32 \times 10^{11}$). Inserting these values in equation [13] thus gives

$$ \frac{VW}{T} = \frac{1.272 \times 10^{12}}{4.32 \times 10^{11}} = 2.94 $$

[14]

Conceptually, this value represents an equal chance of failure at the weld toe and at the root at the ‘design’ level,

2. to use values corresponding to the mean curve for weld throat failure ($6.255 \times 10^{12}$) and the mean curve for Class F_2 ($1.231 \times 10^{12}$). Inserting these values therefore gives

$$ \frac{VW}{T} = \frac{6.255 \times 10^{12}}{1.231 \times 10^{12}} = 5.08 $$

[15]

Given that a suitable value of $\frac{VW}{T}$ (say $x$) can be selected, it is now necessary to relate that value to the corresponding stresses in the plate and the weld throat.

From Fig. 2 it will be seen that the value of I is related to the joint geometry via the parameters $\frac{H}{T_p}$ and $\frac{a_i}{W}$, so that it is possible to define a unique relationship for fillet welds with zero penetration, as shown.

Replotting that relationship in terms of $\xi$ versus $n$ (I) versus $a_i$ versus $\frac{a_i}{W}$, as shown in Fig. 23, it is apparent that, to a close approximation, we can write

$$ (\frac{a_i}{W})^{5.37} I = 0.002 $$

[16]

so that, eliminating I by introducing the value $X$

$$ (\frac{a_i}{W})^{5.37} VW = 0.002 X $$

14
or \( \frac{a_i}{W} = \left\{ \frac{0.002X}{\sqrt{W}} \right\}^{\frac{1}{3.37}} \) \[17\]

Now, if the stress in the plate is \( a_P \), and the stress on the weld throat is \( a_W \), we have, since the total weld throat area in a fillet welded joint with no penetration is \((2X0.7H)\),

\[ a_P T_P = a_W (1.4H) \] \[18\]

so what

\[ \frac{a_W}{a_P} = \frac{1}{1.4 \left( \frac{H}{T_P} \right)} \] \[19\]

But from geometrical considerations (see Fig. 1)

\[ \frac{H}{T_P} = \frac{W-a_i}{2a_i} = 1 - \frac{a_i}{2a_i} \] \[20\]

Therefore, from equations [19] and [20],

\[ \frac{a_W}{a_P} = \frac{2a_i}{W} \left( 1 - \frac{a_i}{W} \right) \] \[21\]

Then, eliminating \( \frac{a_W}{a_P} \) from [21] with [17]

\[ \frac{a_W}{a_P} = \left\{ \frac{0.002X}{\sqrt{W}} \right\}^{\frac{1}{3.37}} \left( 0.002X \right) \frac{1}{3.37} \] \[22\]

\[ 0.7 \left( 1 - \frac{0.002X}{\sqrt{W}} \right)^{\frac{1}{3.37}} = 0.7 \left( \sqrt{W} \right)^{\frac{3}{3.37}} \left( 0.002X \right)^{\frac{3}{3.37}} \]

Which can be evaluated for the relevant values of \( W \) and \( X \). Some typical values are shown in Table 2.

However, \( W \) is not directly proportional to the plate thickness \( T_P \). From geometrical considerations (see Fig. 1)

\[ W = H + 0.5 \, T_P \] \[23\]

and from equation [18]

\[ H = \frac{T_P}{1.4} - \frac{1}{\left( \frac{H}{T_P} \right)} \] \[24\]
so that

\[ T_p = \frac{W}{\left\{0.5 + \frac{1}{1.4} \frac{a_W}{a_P}\right\}} \]

Given that the values of \(\frac{a_W}{a_P}\) for given values of W and X are defined by equation [22] and set out in Table 2, it is easy to derive the corresponding values of \(T_p\) from equation [25]. They are also shown in Table 2, and the resulting relationships between \(\frac{a_W}{a_P}\) and \(T_p\) are shown in Fig. 24. From this diagram it will be seen that, at the basic thickness of 13mm used for deriving the majority of the fatigue design curves in BS 5400, the values of \(\frac{a_W}{a_P}\) corresponding to the two possible values of X specifically considered in this report (namely 2.94 and 5.08, see equations [14] and [15]) are 0.58 and 0.68 respectively. Thus, knowing that the mean and mean - 2 standard deviations fatigue strengths for Class F2 at 2 x 10^6 cycles are \(a_P = 85\) and 60 N/mm^2, it follows that the corresponding strengths required for Class W are as shown in the following table. This assumes, of course, that the standard deviation of Log N for Class W, in terms of \(a_W\) rather than \(a^*\), is the same as for Class F2.

<table>
<thead>
<tr>
<th>Assumed (\frac{a_W}{a_P}) values of x</th>
<th>(\frac{a_W}{a_P}) At (T_p = 13) cycles</th>
<th>Required fatigue strength of Class W at 2 x 10^6 cycles</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Mean</td>
<td>Mean - 2 S.D.</td>
</tr>
<tr>
<td>2.94 (eq. [14])</td>
<td>0.58</td>
<td>49</td>
</tr>
<tr>
<td>5.08 (eq. [15])</td>
<td>0.68</td>
<td>58</td>
</tr>
<tr>
<td>Existing stresses for Class W</td>
<td></td>
<td>57</td>
</tr>
<tr>
<td>(for comparison)</td>
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These strengths may be compared with the existing strengths for Class W at 2 x 10^6 cycles, which are 57 and 43 N/mm^2. It will be noted that these are very similar to the values derived with X = 5.08 (ie equating the means of the scatter bands for toe and root failure).
5. CONVENTIONAL ANALYSIS OF RESULTS

In order to enable a comparison to be made between the available data and the various design stresses suggested above, the results have been replotted on the basis of the nominal weld throat stress in Figs 25 to 47, with the existing mean and mean minus 2 standard deviations S-N curves for Class W also shown in each Figure. It will be seen that, in total, 22 results lie below the mean - 2 S.D. Design line. This is slightly higher than the number which would strictly be anticipated on the basis of a blind application of statistics, namely 11, but 6 of the relevant failures related to specimens which were either galvanised (series 1775) or coated with zinc epoxy primer (series 1779 and 1780) prior to welding. It seems at least possible that those specimens may well have contained additional root defects prior to testing, but it has not been possible to check whether that was actually the case.

A summary of the 22 low results is shown in Fig 48, together with the current mean - 2SD S-N curve for Class W and the corresponding curve for $X = 2.94$ (equation [14]). The curve corresponding to $X = 5.08$ is not shown since it is virtually identical to the Class W curve. It will be seen that for all practical purposes the curve for $X = 2.94$ forms a lower limit to all the results.

It would obviously be unrealistic to assess the scatter in life of the results, expressed in terms of weld throat stress, because of the wide variations in geometry of the various test series. An analysis has, however, been made of the results for those joint geometries for which 20 or more individual results existed. With best fit curves for each geometry forced to slope $m = 3$ (for consistency with Class $F_2$), the results were as shown in Table 3. Compared with a quoted standard deviation of $\xi \eta(N)$ for Class $F_2$ (toe failure) equal to 0.5248, a simple average of the 4 results shown in Table 3 is 0.5658 while a weighted average gives 0.5207. On the basis of these results it does not seem unrealistic to assume that the standards deviations for root failure (Class W) and toe failure (Class $F_2$) are equal (ie 0.5248). This represents an increase in the degree of scatter implicit in the existing Class W design stresses, for which the corresponding standard deviation is 0.4251.

In passing, it should be noted that 3 of these 4 test series gave relatively low standard deviations of $\xi \eta(N)$, up to 0.579. The ‘average’ values were increased mainly by the fact that the fourth series had a high S.D. Of 0.729. It is interesting to speculate that, since the high value was associated with 20mm thick specimens while the lower values related to 10-12mm thick specimens, the higher S.D. Was directly related to the specimen thickness as a result, for example, of a more variable residual stress distribution. Thus, although the residual stresses at a weld toe will always be highly tensile, those at the root may well depend on how well the plate is restrained and the number and size of weld passes. In a multipass fillet weld the root stresses may be compressive and the larger the joint the greater is the possibility of crack closure and an enhanced fatigue life. This also offers a possible explanation for a size effect that is less pronounced in practice than would be inferred from the theoretical fracture mechanics treatment (see Section 7).
6. JOINTS WITH PARTIAL PENETRATION WELDS

The proposed design curve should, of course, also apply to joints with partial penetration but failing the weld throat. In order to check its validity in that situation some readily available test results have been plotted, with the existing Class W curves for comparison, in Figs 49-52. It will be seen that, of the 56 results shown in Figs 49-51, only 3 are lower than (and one is on) the mean minus 2 standard deviations curve. Of the results in Fig. 52, however, the majority are all below the proposed design curves, but all refer to weld throat stresses above 175 N/mm² and hence to short life failures. Equally it will be noted that two of the three low results referred to previously were also at high stress (265 N/mm²). Given that all these ‘unsafe’ results were at stresses above those which would normally be used for weld metal, it seems reasonable to conclude that the test results for partial penetration welds support the use of the proposed curve for the design of such joints.
7. THICKNESS CORRECTION FACTOR

As can be seen from Table 2 and from Fig. 24, the theoretical analysis shows that, as plate thickness \( T_p \) increases \( \frac{a_w}{a_P} \) decreases. Indeed, it is easy to deduce from Fig. 24 that, for a particular value of \( X \),

\[
(\frac{a_w}{a_P}) T_p^{0.15} = \text{constant}
\]

[26]

However, for failure from the weld toe in the plate, it has been found that fatigue strength tends to decrease with increasing plate thickness and in the Guidance Notes [1a] it is stated that the design stress should be decreased with increasing plate thickness such that

\[
a_P = S_B \left(\frac{T}{T_B}\right)^{0.25}
\]

[27]

Where \( a_P \) is the fatigue strength of a joint with thickness \( t \) and \( S_B \) is the fatigue strength of a joint of the basic thickness \( T_B \). For the type of joint under consideration \( t_B \) is assumed to be 22mm, even though that is illogical.

Now, from equation [26] we have

\[
(\frac{a_w}{a_P}) = (\frac{a_w}{a_{PB}})(\frac{T_B}{T})^{0.15}
\]

[28]

so that, substituting from [27] into [28]

\[
a_w = (\frac{a_w}{a_{PB}}) S_B \left(\frac{T_B}{T}\right)^{0.4} = (a_{PB})(\frac{T_B}{T})^{0.4}
\]

[29]

as the required thickness correction factor for Class W based simply on theoretical considerations.

Unfortunately, it cannot really be said that the test results support that conclusion, but that may, in part, be due to the fact that extremely few of the results relate to ‘thick’ specimens. As far as specimens thicker that the current ‘basic’ thicknesses (\( t_B = 22 \text{mm} \)) are concerned, Figs 32, 33, 36 and 42 show results for specimens with plate thicknesses of 25, 38, 25 and 32mm respectively and all those results are ‘safe’ without the applications of any thickness correction factor. The only results for ‘thick’ specimens which are currently unsafe are those shown in Fig 34 for 30mm thick specimens. If the correction factor derived in equation [29] were applied to the Class W mean - 2 standard deviations curve it would be reduced from 116 and 43 N/mm\(^2\) at \( 10^6 \) and \( 2 \times 10^6 \) cycles respectively to 102 and 38 N/mm\(^2\). The relevant curve is indicated on Fig. 34. It will be seen that only the results at high stress and less than \( 10^4 \) cycles are then ‘unsafe’.

In this context it is of interest to note that, as part of the UKOSRP-I test programme, an investigation was carried out with the objective of defining the optimum weld size of some joints with load-carrying fillet and partial penetration...
welds in specimens 25 and 38mm thick. The results are summarised in Table 4, which indicates that the recommended weld throat stresses varied from 0.94 to 1.13 times the corresponding plate stresses. It will be noted that these are far higher than those corresponding either to the current Class W curves or to curves based on \( X = 2.94 \) or 5.08.

It would clearly be useful to investigate the need for a thickness correction factor to Class W in more detail by carrying out some tests on a range of joints fabricated from thicker material. In the meantime it is strongly suggested that, in view of the scanty evidence, the thickness correction for Class W should be no more severe than for Class \( F_2 \).
8. LONGITUDINAL LOAD-CARRYING FILLET WELDS

No attempt has been made to carry out a similar analysis for joints with longitudinal fillet welds. Relatively few test results have been obtained for such joints and the great majority of those involved failures in the plate rather than in the weld throat. However, in a few specimens tested by Gurney (24) there were small subsidiary cracks in the weld throat near the weld ends. More recently, in tests carried out by Chapeau and Plumier (15), it was reported that weld failures had been obtained, but unfortunately the specimen dimensions were not reported in sufficient detail to enable the relevant weld throat stress to be calculated and, in addition, it is not clear which specimens failed in the weld and which in the plate.
Another form of load-carrying fillet welded joint is the continuous longitudinal (i.e. parallel to the direction of stress) fillet weld, such as often exists at the web to flange joint of fabricated girders. As far as is known no failures of such welds in the weld throat along the length of the weld have ever been reported either in service or in laboratory tests on welded beams. Under normal circumstances, if failure occurs, it consists of cracking transverse to the weld direction initiated either at weld ripples or weld defects and the strength is high.

Recently, however, some tests have been reported (25) on an equivalent joint. The specimens consisted of 108mm diameter tubes fillet welded to end plates and subjected to alternating (R=-1) torsion. This did result in shear failure in the weld. The results, for specimens fabricated from two different (but no details given) steels, are shown in Figs 53 and 54. However, since there was little difference between the two sets of results all were analysed together, ignoring the test results giving lives greater than $6 \times 10^6$ cycles, and the corresponding best-fit mean and mean-2 standard deviations S-N curves are also shown in Figs 53 and 54. In addition the best-fit curves with slope forced to m=3 are also shown.

For these particular tests it seems clear that the slope was much shallower than 3.0 and there may well be a temptation to introduce a new Class to cover such joints. This should be resisted, at least for the moment, because

a) The tests were at $R = -1$ and it is known that stress ratio can affect the slope, depending to a large extent upon residual stresses;

b) As far as is known there are no confirmatory test results available from a different source;

c) The introduction of a new S-N curve with a different slope would tend to complicate design and cause confusion.

With the last point in mind it may be noted that the mean and mean -S.D. Strengths at $2 \times 10^6$ cycles, assuming m=3, are 107 and 74 N/mm$^2$. These lie midway (in both cases) between Classes E and F. It is therefore proposed that this type of weld should be classified as F.
10. CONCLUSIONS AND RECOMMENDATIONS

Some 500 test results for transverse load-carrying fillet welds failing in the weld throat have been analysed. It has been shown that, theoretically, an equal chance of failure at the weld toe and at the weld root at the design level is obtained by making the basic mean and means-2SD strengths for Class W at $2 \times 10^6$ cycles equal to 49 and 35N/mm$^2$ respectively. Further analysis of the results showed that, in fact, such a design curve effectively formed a lower bound to all the test results.

It is suggested that, in fact, the strengths at $2 \times 10^6$ cycles should be made equal to 50 and 36 N/mm$^2$ respectively, since that is a small change which would make the design curve coincide with the corresponding strength proposed for Eurocode No 3: Common Unified Rules for Steel Structures, and there is obviously some benefit in moving towards a likely European Standard.

On this basis the design equation for the revised (mean-2SD) Class W curve would be

$$\log N = 10.970 - 3 \log S$$

with a standard deviation (SD) equal to 0.214 where $N$ is the predicted number of cycles to failure under stress range $S$. Alternatively the equation can be expressed as

$$S^{1.3} N = 9.331 \times 10^{10}$$

Results from 56 tests on joints with partial penetration welds (failing in the weld throat) showed that the proposed design curve would also be acceptable for the design of those joints.

Although there are theoretical reasons for believing that the Class W design stresses should be subject to a thickness correction factor which is more severe than that for toe failure, the available experimental results do not support that conclusion. It is recommended that some experimental tests should be put in hand to clarify the situation but, in the meantime, that any thickness correction introduced at this stage should be no more severe than for Class F2.

As far as continuous longitudinal fillet welds (e.g. Web-to-flange welds) are concerned, it is proposed that they should be included in Class F, the relevant stress being the range of weld throat shear stress. There is no evidence regarding the possible existence of a thickness effect in such joints but there are no obvious theoretical reasons for expecting one.
11. REFERENCES

1. BS 5400, Part 10, and HSE Guidance Notes.

1a. HSE Guidance Notes.


18. Denny A K and Jubb J E M. ‘Fatigue crack propagation in submerged arc


<table>
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<th>CAR No</th>
<th>Ref</th>
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<th>$H$</th>
<th>$a_i$</th>
<th>$W$</th>
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<th>$a/W$</th>
<th>$l$</th>
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TABLE 2 VALUES OF $\frac{a_w}{a_p}$ AND $T_p$ FOR A RANGE OF VALUES OF W AND 3 POSSIBLE VALUES OF X

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TABLE 3 SUMMARY OF RESULTS FOR INDIVIDUAL JOINT GEOMETRIES FOR WHICH 20 OR MORE SPECIMENS HAVE BEEN TESTED

<table>
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<tr>
<th>Plate thickness</th>
<th>Weld leg</th>
<th>No. of results</th>
<th>SD of $\zeta$ (nN)</th>
<th>Mean $10^3$</th>
<th>Mean-2 SD $10^3$</th>
<th>Mean $2 \times 10^6$</th>
<th>Mean-2 SD $2 \times 10^6$</th>
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<td>53</td>
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Current Class W (for comparison) 154 57 116 43

Note: The above analyses are based upon the assumption that the S-N curves have slope $m=3$

TABLE 4 SUMMARY OF RECOMMENDED WELD SIZES DETERMINED FOR 25 AND 38MM THICK AS-WELDED SPECIMENS IN UKOSRP I

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<th>Plate thickness</th>
<th>Degree of weld penetration</th>
<th>Recommended leg length, mm</th>
<th>Corresponding $\frac{aw}{ap}$</th>
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<tr>
<td>38</td>
<td>zero</td>
<td>24</td>
<td>1.13</td>
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</table>
Fig 1. Joint geometry.
Fig 2. Values of $I$ as a function of initial crack size and joint geometry.
Fig 3. Results reported by McFarlane and Harrison

Fig 4. Results reported by Stallmeyer et al.
Fig 5. Results reported by Haibach[6]

Stress parameter $\Delta \alpha^*$

Endurance (cycles) Transverse load carrying fillet weld data

CAR number(s) : 679-681, 685, 687-689
FS number(s) : 2172

Fig 6. Results from ECSC report EUR 5357 E[7]
**Fig 7. Results from ECSC report F4.1/73.**

**Fig 8. Results reported by Booth (9) (UKoSRP 1).**
Fig 9. Results reported by Nagai et al.\(^\text{(10)}\)

\[\Delta \sigma^* \text{ vs. Endurance (cycles)}\]

CAR number(s): 1614, 1839
FS number(s): 9879

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Fig 10. Results reported by Lienard.\(^\text{(11)}\)

\[\Delta \sigma^* \text{ vs. Endurance (cycles)}\]

CAR number(s): 1625, 1650
FS number(s): 9812
Fig 11. Results reported by Recho and Brozzetti.\(^{(12)}\)

Fig 12. Results reported by Gregory.\(^{(13)}\)
CAR number(s): 1778
FS number(s): 9813, 9815

Fig 13. Results reported by Drayton\(^{(14)}\)

CAR number(s): 1907, 1908
FS number(s): 9820

Fig 14. Results reported by Frank\(^{(2)}\)
Fig 15. Results reported by Chapeau and Plumier. (12)

Fig 16. Results from NRIM data sheet No 18. (16)
Fig 17. Results reported by Ouchida and Nishioka (17)

Fig 18. Results reported by Denny and Jubb (18)
Fig 19. Results reported by Shingai and Imamura.\textsuperscript{(19)}

Fig 20. Results reported by Yamaguchi et al.\textsuperscript{(20)}
Fig 21. Results reported by Ohta and Eguchi.\textsuperscript{[14]}

Fig 22. Results reported by Andrews.\textsuperscript{[22]}
Fig 23. Values of I for fillet welds with zero root penetration.
Fig 24. Values of $\frac{\sigma_w}{\sigma_p}$ as a function of $T_p$ and $X$. 
Fig 25. Results reported by McFarlane and Harrison.\(^{(4)}\)

Fig 26. Results reported by Stallmeyer et al.\(^{(6)}\) (\(R=0\)) (W.l. Ref CAR 650 and 651).
Fig 27. Results reported by Haibach \(^{(6)}\) \((R=0)\).

Fig 28. Results from ECSC report EUR 5357 E \(^{(7)}\) \((R=0.1)\) (W.i. Ref CAR 790).
Fig 29. Results from ECSC report EUR 5357 E\textsuperscript{(7)} for specimen of steel E460 (W.I. Ref CAR 817).

Fig 30. Results from ECSC report F4.1/7.3\textsuperscript{(8)} (R=0.15) (W.I. Ref CAR 832-834)
Fig 31. Results obtained in ECSC programme F4./7.3\(^{(6)}\) at $R=0.1$
(W.I. Ref CAR 855 and 879).

Fig 32. Results obtained by Booth for 25 mm thick specimens.\(^{(6)}\)
Fig 33. Results obtained by Booth for 38 mm thick specimens.\(^9\)

Fig 34. Results reported by Nagai et al.\(^{10}\)\(^{\text{(R=0)}}\) (W.I. Ref CAR 1614 and 1839).
Fig 35. Results reported by Lieurade.\(^{(11)}\)

N.B. Different scales of axes of graph.

Fig 36. Results reported by Recho and Brozzetti.\(^{(12)}\)
Fig 37. Results reported by Gregory $^{(13)}$ ($R=0$).

Fig 38. Results reported by Drayton $^{(14)}$ ($R=0$).
Fig 39. Results reported by Frank (2) (W.I. Ref CAR 1907-8).
N.B. Different scales of axes of graph.

Fig 40. Results reported by Chapeau and Plumier (15) \((R=0.60-0.75)\).
Fig 41. Results from NRIM (Japan) Data sheet No 18\textsuperscript{(16)} (W.i. Ref CAR 2079).

Fig 42. Results reported by Ouchida and Nishioka.\textsuperscript{(17)}
Fig 43. Results reported by Denny and Jubb \(^{(18)}\) (R=0).

Fig 44. Results reported by Shingai and Imamura \(^{(19)}\) (W.I. Ref CAR 3167).
Fig 45. Results reported by Yamaguchi, Terada and Nitta\textsuperscript{(20)} (R = 0).

Fig 46. Results reported by Ohta and Eguchi\textsuperscript{(21)} (R = 0).
Fig 47. Results reported by Andrews.\(^{(22)}\)

Fig 48. Summary of test results falling below existing class W mean - 2 SD.
Fig 49. Results for partial penetration welds obtained by Booth.\(^{(9)}\)

Fig 50. Results for partial penetration welds \((T=16\text{ mm})\) obtained by Ouchida and Nishioka.\(^{(17)}\)
Fig 51. Results for partial penetration welds (T=32 mm) obtained.

Fig 52. Results for partial penetration welds obtained by Tsuji and Ogawa.\(^{(23)}\)
Fig 53. Results for tube to plate fillet welds tested in alternating (R=−1) shear (Steel 00).

Fig 54. Results for tube to plate welds tested in alternating (R=−1) shear (Steel 00.29).
APPENDIX A

STRESS INTENSITY FACTOR FOR TRANSVERSE LOAD-CARRYING FILLET WELDS FAILING FROM THE WELD ROOT

The value of K for this type of joint, assuming a root crack of total length 2a, has been shown by Frank (2) to be approximately

\[ K = a_p \sqrt{\pi a} \left[ A_1 + A_2 \left( \frac{W}{T_p} \right) \right] \left( \frac{\pi a}{W} \right)^{3/2} \left( 1 + \frac{H}{T_p} \right) \]

where \( W, H \) and \( T_p \) are defined in Fig. 1 and where the coefficients \( A_1 \) and \( A_2 \) are:

\[ A_1 = 0.528 + 3.287 \left( \frac{H}{T_p} \right) - 4.361 \left( \frac{H}{T_p} \right)^2 + 3.696 \left( \frac{H}{T_p} \right)^3 - 1.874 \left( \frac{H}{T_p} \right)^4 + 0.415 \left( \frac{H}{T_p} \right)^5 \]

\[ A_2 = 0.218 + 2.717 \left( \frac{H}{T_p} \right) - 10.171 \left( \frac{H}{T_p} \right)^2 + 13.122 \left( \frac{H}{T_p} \right)^3 - 7.755 \left( \frac{H}{T_p} \right)^4 + 1.783 \left( \frac{H}{T_p} \right)^5 \]

The expression \( [A_1 + A_2 \left( \frac{W}{T_p} \right)] \) is an approximation to a polynomial expression and for \( 0.2 < \left( \frac{H}{T_p} \right) < 1.2 \) the resulting solution for K is within 2% of the accurate solution.