FATIGUE DESIGN AND ASSESSMENT OF WELDED JOINTS

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ABSTRACT

Fatigue design and assessment methods for welded components and structures are reviewed with particular reference to the significance of fatigue crack growth and the role of fracture mechanics in their formulation. Current developments and future research needs are discussed.

KEYWORDS

Fatigue, welds, design, crack propagation, fracture mechanics, cumulative damage, fatigue life prediction.

INTRODUCTION

Fatigue is the primary source of failure in welded structures and comprehensive design rules are needed to avoid failures. Welds are particularly susceptible because of the severe stress concentrations and flaws they introduce. Fracture mechanics is well known as the primary method for analysing the effects of cracks in structures, leading to such practical applications as fitness-for-purpose assessments of flaws, estimation of remaining life and establishment of in-service inspection intervals. What is perhaps less well known is the extent to which fracture mechanics principles and techniques have been used in establishing fatigue design rules for welded components and structures, and the ways in which it is being used to address current design-related problems. This paper reviews fatigue design rules for weldments and considers how fracture mechanics is or could be used to address major deficiencies or new developments.

FATIGUE DESIGN RULES FOR WELDMENTS

Three key features of welded joints dominate their fatigue lives (Gurney, 1978; Maddox, 1991):

(a) Geometric stress concentrations due mainly to abrupt increases in section.

(b) Welding flaws, particularly those situated within fields of geometric stress concentration (e.g. the non-metallic crack-like intrusions found at weld toes in steels).

(c) High (up to yield) tensile residual stresses, which have the effect of producing high tensile mean stress conditions regardless of the applied loading.

To ensure that the full effect of these features is allowed for in design, most fatigue design rules consist of a series of S–N curves based on data obtained from constant amplitude fatigue tests on actual weldments (Maddox, 1992). Examples are given in Fig.1, in which use is made of the commonly used classification method of specifying design curves in terms of the fatigue strength (in MPa) at $2x10^6$ cycles. Since the stress concentration effect of the welded joint geometry is included, S refers to the nominal stress adjacent to the weld detail. Furthermore, to allow for the influence of residual stresses, the full stress range is used. The S–N curves are used in conjunction with Miner's rule to design structures subjected to variable amplitude loading.

An important consequence of the geometric stress concentrations associated with most welded joints, the severity of which is usually compounded by the presence of welding flaws (Watkinson et al, 1971), is that fatigue cracks readily initiate and the life is dominated by fatigue crack growth (Maddox, 1991). This accounts for the drastic reduction in fatigue life resulting from the presence of a weld (even one which is not transmitting the applied load, see Fig.1). It also explains why fracture mechanics is so relevant to the fatigue assessment of weldments. Specific aspects of fatigue design rules for weldments which are influenced by the fact that fatigue crack growth dominates their lives, and how they are changing in the light of new requirements and developments, are addressed in this paper.

SIGNIFICANCE OF CRACK GROWTH IN FATIGUE DESIGN

Form of S–N Curve

If the fatigue life of a welded joint consists mainly of crack growth, the S–N curve can be calculated by integrating the crack growth law. Adopting the basic Paris law and the usual fracture mechanics terminology:

$$\frac{da}{dN} = C\,(\Delta K)^m \qquad [1]$$

The resulting S–N curve is predicted to be:

$$S^m\,N = A \qquad [2]$$

where m, A and C are constants and ΔK = stress intensity factor range. In other words, the S–N curve is linear on a log–log basis with a slope equal to that of the Paris law. As a consequence of this, most design S–N curves for welded joints are taken to be parallel with a slope compatible with the fatigue crack growth law for the material. Since m is approximately 3 for most materials, S–N curves with slopes of 3 are widely adopted.

Based on the early estimate of ΔK_0 = 3MPa√m for steel and current stress intensity factor solutions for weld toe cracks, the fatigue limit for fillet welds was estimated to be 40MPa. From available S–N data, this corresponded to an endurance of approximately 10^7 cycles, the location of the fatigue limits in all British Standard. Other codes use the less conservative

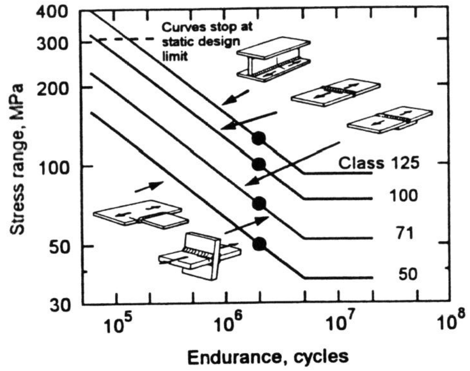


Fig.1 Typical design S-N curves and corresponding weld details, showing site for fatigue cracking and direction of loading considered.

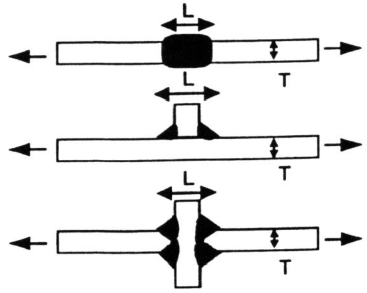
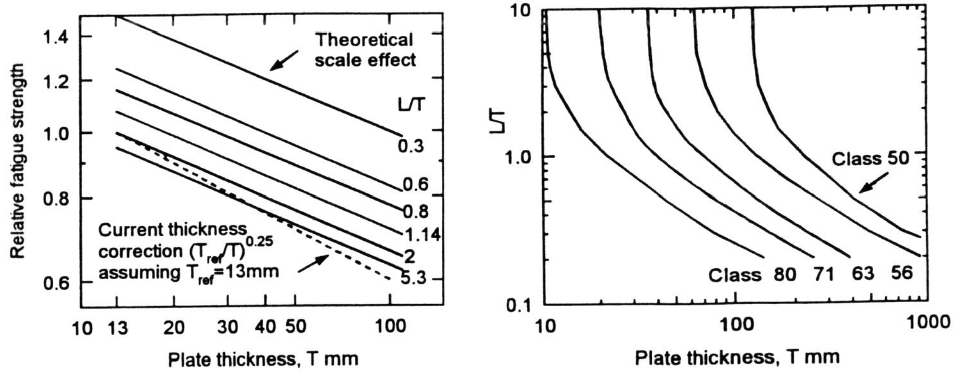


Fig.2 Welded joint geometries considered in fracture mechanics analysis of scale effect.



a) Thickness effect.

b) Welded joint dimensions corresponding to design S-N curves.

Fig.3 Results of fracture mechanics analysis of scale effect in welded joints of the type illustrated in Fig. 2.

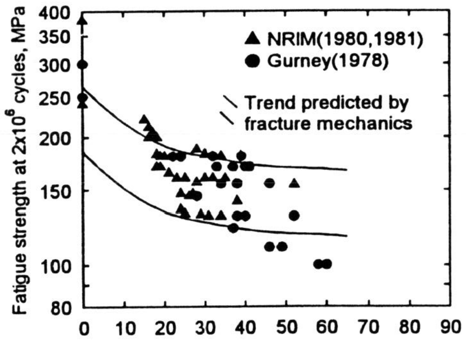


Fig.4 Butt weld toe angle versus fatigue strength

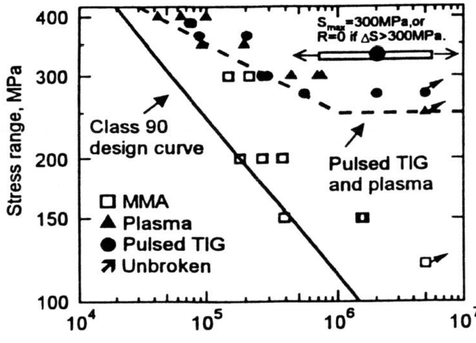


Fig.5 Improved fatigue performance of TIG and plasma butt welds in 3mm thick steel.

values of stress range at 5 or even 2×10^6 cycles. However, based on more up-to-date information it would not be unreasonable to adopt a **more** conservative approach. Allowing for tensile residual welding stresses, the lower bound value of ΔK_0 for steel is $2MPa\sqrt{m}$ (BSI, 1991) which, for a fillet weld, results in a fatigue limit of around 35MPa, or even lower for cover-plated beams. Whatever the value, it will be clear that the design of weldments on the basis of the constant amplitude fatigue limit will rarely be practicable, design stresses being only 10% or less of the static strength of the material.

For most practical cases, the precise value of the constant amplitude fatigue limit is irrelevant, since fatigue loading normally gives rise to variable amplitude stressing. As soon as a fatigue crack has initiated under stresses greater than the fatigue limit, the effective fatigue limit is reduced and hence stresses below its original value become damaging. This is recognised in most codes and, based partly on fracture mechanics analysis, the S–N curves are extrapolated beyond the constant amplitude fatigue limit at a shallower slope (e.g. m+2). However, recent studies indicate that even this approach may be unconservative because the fatigue limit immediately a fatigue crack has initiated is significantly lower than that of the original weld (Marquis, 1996). The current suggestion is to introduce the slope change to m+2 at $N=2\times10^7$ cycles rather than 10^7 cycles. The practical implication of this will depend on the load spectrum, being most significant for cases in which the main fatigue loading produces stresses near or below the constant amplitude fatigue limit.

INFLUENCE OF JOINT GEOMETRY

Even though most of the design S–N curves for weldments refer to crack growth from the toe of a weld, a number of curves are needed to allow for differences in the magnitude of the geometric stress concentration due to the welded joint geometry. Thus, a transverse butt weld has a higher but parallel S–N curve to that for a fillet weld, while a long attachment (e.g. cover plate) introduces a higher stress concentration factor than a short one (e.g. fillet welded stiffener). The separation of the design curves reflects differences in experimental S–N data. However, parametric studies based on fracture mechanics models of the fatigue behaviour of welded joints which fail from the weld toe have identified geometric features which contribute to the stress concentration factor and allowed more rational rules to be developed. The impetus for such studies was provided by the introduction of a design penalty related to one specific feature of geometry not recognised when the design rules were first produced, namely plate thickness (T). The effect of joint geometry is included in the stress intensity factor equation for a weld toe crack in the parameter M_k (i.e. $\Delta K=Y\ M_k\ S\sqrt{\pi a}$, where Y is a function of crack size and shape and of loading) (Maddox, 1975). Early solutions drew attention to a possible thickness effect in that M_k decreased with increased a/T. Thus, cracks were influenced by the stress concentration due to the weld to a greater depth in thick than thin plates. Examination of relevant fatigue data confirmed such a thickness effect and provided the basis of the now widely used design penalty $(T_{ref}/T)^{0.25}$, where the reference thickness T_{ref} lies between 13 and 25mm, depending on the database upon which the design S–N curves were based (Gurney, 1979). However, in practice it is very difficult experimentally to explore the effect of varying only one dimension in a welded joint, due to the difficulty of keeping all other relevant dimensions, notably the local weld toe geometry, constant. Therefore, parametric studies based on fracture mechanics are potentially more reliable. Closer examination of M_k solutions for typical weld geometries (see Fig.2) has shown that they also depend on other dimensions, notably L but also the local weld toe geometry characterised by the weld toe angle and radius. Keeping the local geometry constant, fracture mechanics analysis (Maddox, 1987) and supporting experimental data for welded steel (Gurney, 1991) and aluminium alloy (Maddox, 1995) suggest a size effect incorporating both plate thickness and "attachment size" L, as illustrated in terms of the relative fatigue strength, defined as the ratio fatigue strength at 2×10^6 cycles for T_{ref} to that for thickness T, in Fig.3(a). The present design penalty is close to what could be regarded as the worse case, high L/T and T and L increasing in proportion to one another. Clearly, it is too severe in the case of thick members with thin attachments. From fracture mechanics, an alternative correction for the scale effect is $(T_{ref}/T_{eff})^{0.25}$, where $T_{eff}=0.5L$ if $L/T < 2$, or T if $L/T > 2$. Apart from relaxing the current thickness effect design penalty, the new information provides the possibility of rationalising design rules in general to take closer account of joint geometry. An example is illustrated in Fig.3(b), which shows curves relating L/T and T for particular design S–N curves all of which refer to weld details of the type illustrated in Fig.2 (Maddox, 1987). Indeed, the same approach can be used to rationalise a difference in fatigue performance which has not been explained previously. This is the consistently low fatigue performance obtained from stressed members with edge, as compared with surface, attachments (see Fig.1). The fact that what amounts to an edge crack rather than a semi-elliptical surface crack develops is relevant, but not sufficient to explain the reduction in fatigue life. However, it can be understood in terms of the above scale effect if T is defined as the plate dimension through which a fatigue crack will propagate to failure. Thus, if the attachment is on the plate surface, T = plate thickness, while for edge attachments T = plate width. Most fatigue data obtained from edge attachment details were 100–150mm wide with similar or greater lengths of attachment. From Fig.3(b) these should correspond to Class 50 or 56, which is indeed the case. Such correlation clearly opens the way to a more comprehensive classification system related to fewer S–N curves but in which relevant joint dimensions are taken into account when defining the stress range.

EFFECT OF WELD PROFILE

It is clear that the stress concentration at the toe of a weld is influenced by weld shape, notably the weld toe angle and radius. By suitable choice of process or technique, some control over weld shape is possible and therefore this is seen by many as a practical approach to improving fatigue performance, perhaps justifying even higher design curves. Indeed, this view is taken in the US in that the AWS fatigue rules allow the use of higher design curves for welds which meet particular improved profile criteria, which are checked partly using the well known "dime test". However, there is little experimental evidence for such benefit and indeed fracture mechanics analysis suggests that it is not as great as might be expected. A key problem is the fact that even with good profile, there is still a flaw at the weld toe from which a fatigue crack will grow with the result that it is the effect of weld shape on crack growth which is relevant. M_k depends on weld angle and toe radius, but as in the case of other dimensions which affect it, the effect decreases as the crack depth increases. The result is rather a weak influence of weld shape, as illustrated for weld toe angle in the case of transverse butt welds in Fig.4. A specific study of the effect of achieving the AWS improved profile in tubular joints (Maddox et al, 1995) showed that there was no significant difference in fatigue performance between joints carefully welded to meet the specification and others with extremely poor profiles. It is clear that of greater importance than weld profile is the reduction or avoidance of weld toe flaws. Undoubtedly the most reliable way to achieve this is to remove them after welding, for example by toe grinding or TIG dressing (remelting). Such measures have the effect of introducing a significant fatigue crack initiation period, the process which is more influenced by local geometry than crack growth. There also seems to be scope for achieving improved fatigue properties from welds made by TIG welding. Early studies of the flaws produced at weld toes by arc welding showed that those associated with TIG welding were far less severe than those associated with other arc welding processes (Watkinson et al, 1971). This led to the idea of TIG dressing to improve fatigue performance. Recent work has confirmed that TIG, and plasma, welds can indeed give much greater fatigue lives than similar joints made by other arc welding processes (Fig.5). Apart from reduction in the severity of weld toe flaws, the processes seem to be capable of achieving low weld toe angles and favourable toe radii. However, it has to be recognised that the scope for using these processes in production is limited by their slow speed, although both methods can be automated and this may extend their range of application.

EFFECT OF MATERIAL STRENGTH

One of the most important consequences of the dominance of fatigue crack growth in the lives of welded joints is the fact that fatigue life does not increase with increase in material strength, rate of crack growth being insensitive to material tensile strength. This contrasts with unwelded material (Fig.6). This principle was established over 30 years ago and yet the quest for methods for utilising high strength steels to advantage in fatigue loaded welded structures still receives considerable attention. The most promising finding is that some post-weld improvement techniques, in particular those which introduce a significant fatigue crack initiation period such as weld toe grinding, provide greater benefit for high strength than low strength steels. However, the dependence on steel strength is still rather weak compared with that seen for unwelded and even notched material, as illustrated using published data reviewed by Bignonnet (1987) and Haagensen (1996) in Fig.6, reflecting the severe stress concentration represented by the welded joint geometry.

There might also be a benefit to be gained from the use of high strength materials for structures which experience some load spectra. In particular, higher strength material may be required to carry a small number of relatively high stress cycles. This depends on the spectrum, but Fig.7 shows examples of cases where higher strength would certainly be required to avoid yielding in a low strength steel. It has also been suggested that spectra which give rise to crack growth retardation would result in higher fatigue lives in welded high strength materials, on the basis that beneficial compressive residual stresses would be higher in a high strength material. However, evidence for this is extremely limited. Thus, at this stage there seems little scope for changing the current situation in fatigue design rules that they are applicable to any strength of material of the type specified.

CUMULATIVE DAMAGE

One important area where research is needed is in fatigue life prediction under variable amplitude loading. Potential error in the use of Miner's rule due to the damaging effect of stress cycles below the original fatigue limit has already been discussed, but there is also a problem in the basic validation of Miner's rule for welded joints. Increasingly, it is being found from fatigue tests carried out under random loading conditions that Miner's rule is unsafe, by up to a factor of 2 of fatigue life (Gurney and Maddox, 1990). This error may seem small compared with the extent of scatter seen in fatigue data, but it is associated with an analysis method rather than experimental data it is important to understand how it arises. Clearly, errors can arise in the interpretation of a random stress history (i.e. cycle counting) and in the method of allowing for low stresses. However, these were not relevant in the cases referred to. The problem seems to be associated with wide band loading conditions, the damaging effect of small stress fluctuations being greater in a variable amplitude sequence than under constant amplitude loading. It is thought that this situation arises because the crack closure conditions for a given stress fluctuation are different under variable and constant amplitude loading. On

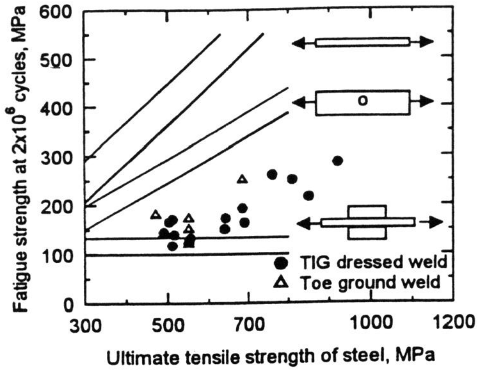
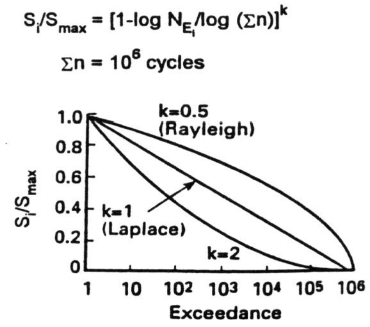


Fig.6 Effect of steel strength on fatigue strength (improved welds based on increase in strength assuming 100MPa for as-welded joint)



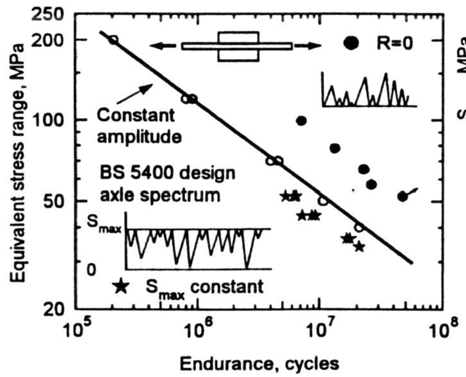


Fig.8 Effect of mean stress on fatigue of fillet welds in steel under spectrum loading (Tilly,1985)

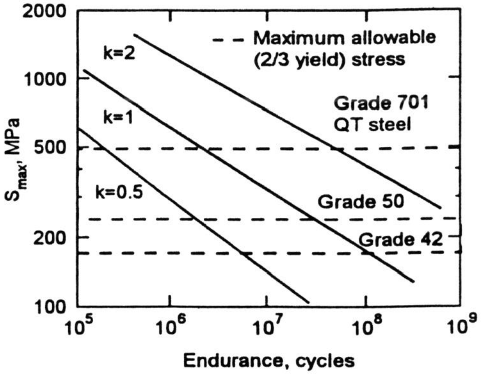


Fig.7 Predicted S-N curves for Class 71 welds under various stress spectra.

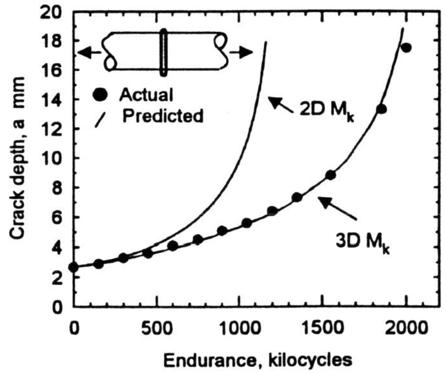


Fig.9(a) Comparison of actual and predicted crack growth in 609mm dia. x 20mm wall thickness girth welded tube.

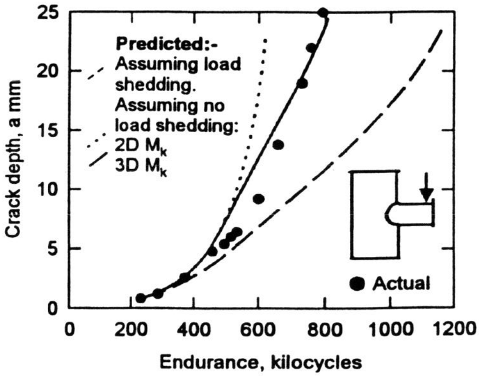


Fig.9(b) Fatigue crack growth from weld toe in 25mm thick chord of 457mm diameter tubular T-joint under in-plane bending (Maddox,1996) this basis, it would be expected that constant amplitude data obtained under conditions where the crack tip will always be open, that is at high tensile mean stresses, would always produce safe fatigue life estimates using Miner's rule. However, Fig.8 shows data (Tilly, 1985) obtained under constant and variable amplitude loading conditions in which the maximum stress was maintained constant at a high tensile level, expressed in terms of the equivalent stress range $[\Sigma S_i^3 n_i/\Sigma n_i]^{1/3}$. The results should agree if Miner's rule is correct, whereas in fact the rule would have over-estimated the high S_{max} lives. In contrast at R=0 the spectrum led to lives exceeding those predicted by Miner's rule due to crack growth retardation. Fundamental study of crack growth under variable amplitude loading is needed to resolve this problem.

FATIGUE LIFE PREDICTION

Techniques for predicting the fatigue lives of welded joints using fracture mechanics were established in the 1960s and subsequently incorporated into BS PD 6493 (BSI, 1991). Since then the basic method has remained unchanged, although recently improvements have been made to the input parameters (Stacey et al, 1996). One significant improvement has been the introduction of more accurate and comprehensive K-solutions. The most common practical problems concern the growth of either semi-elliptical surface cracks (e.g. from weld toe) or elliptical embedded cracks (e.g. from weld defects). The Y solutions to allow for crack front shape (a/2c) and depth (a/T) due to Newman and Raju (1984) are still widely accepted as the best of the closed form solutions. However, improvements have been made to the correction M_k. Although reasonably comprehensive solutions relating M_k to welded joint dimensions were produced in the mid-1980s and incorporated in PD 6493, they were obtained by 2D finite element analysis and then assumed to be applicable to the actual 3D case (Maddox and Andrews, 1990). One important assumption is that M_k for growth in the c-direction, M_{kc}, is constant and equal to M_{ka} at a specified crack depth, usually a=0.15mm, but this has always been recognised as a compromise. As 3D M_k-solutions became available in the late 1980s, it proved possible to derive an approximate correction of the 2D M_k solutions (Pang, 1990):

$$M_{kc} = M_{ka} + 1.15 \exp(-9.74\, a/T) \qquad [3]$$

The value of this simple correction is illustrated in Fig.9 which shows the good correlation possible between predicted and actual crack growth rates in two particular tubular configurations, a girth weld and a T-joint. It can be expected that more and more 3D solutions will be produced, but meanwhile Eq.[3] provides a useful approximation.

One controversial aspect of the analysis of tubular joints is the justification for assuming load shedding due to crack growth. This has the effect of reducing the influence of the bending stress component, such that:

$$\sigma_b = \sigma_{bo} (1 - a/T) \qquad [4]$$

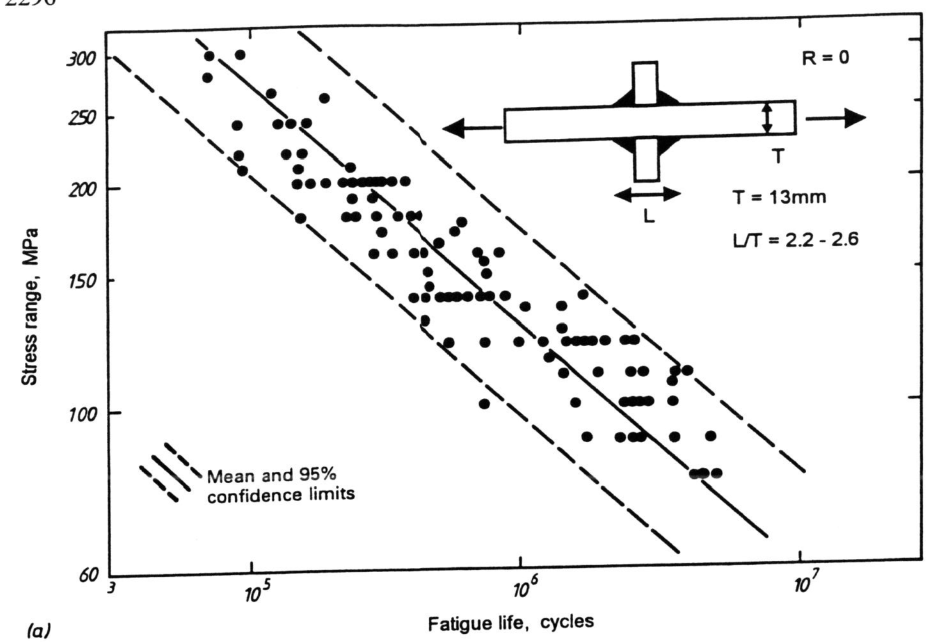
where σ_b and σ_{bo} are the bending stress components in the cracked and uncracked joints, respectively (Aaghaakouchak et al, 1989). Analysis of T-joints in out-of-plane bending showed that this correction improved fatigue life predictions (Tubby, 1995), but analysis of T-joints in under in-plane-bending (Maddox, 1996) did not (Fig.9(b)).

In view of the potential scope in a fracture mechanics analysis for varying the input parameters to allow for specific material properties, geometry and loading, it is natural to consider the possibility of using fracture mechanics instead of S-N curves for design. However, in most practical cases this is not feasible. The problem concerns the definition of the initial flaw size, a_i. Logically, this would be related to non-destructive testing (NDT) capability, for example the depth of surface crack which could be detected reliably. However, such an approach presents problems when considering welded joints, particularly the important weld toe region. As noted earlier, evidence from metallurgical examination of welds in ferritic steels (Watkinson et al, 1971) indicates that fatigue cracks propagate from pre-existing flaws which range in depth from 0.05-0.4mm, with an average of around 0.15mm. Such flaws are undetectable by NDT. Invoking a realistic detection limit of say 1mm crack depth, which is probably still optimistic, means that much lower fatigue lives are predicted than those achievable. For example, 50-70% of the fatigue life of a fillet weld may be used producing a 1mm deep crack.

An alternative approach is to calculate the number of cycles required to initiate a fatigue crack, for example using the local strain method. However, such an analysis does not predict the size and shape of the initiated fatigue crack, dimensions which have a significant influence on predicted fatigue life.

A third possibility is to assume the flaw depth, using metallurgical evidence of the kind referred to earlier. The potential benefit for a situation in which the actual crack growth properties of the material are known is illustrated by comparing actual fatigue data, for transverse fillet welds, with fatigue lives predicted using fracture mechanics. The fatigue data (Fig.10(a)) were all obtained at TWI over a period of years, from similar specimens (M_k the same) in structural steels. Thus, they would not have been influenced by laboratory-to-laboratory variations in test method and environment, in which case is seems reasonable to assume that the scatter is mainly due to variations in material properties and initial flaw size. The input values for the fracture mechanics calculations were a_i=0.05 to 0.4mm; a_f=plate thickness; m=3; C=1.64, 0.9 and 3×10^{-13}, the mean and 95% confidence limits enclosing published data for structural steels assuming the Paris law (i.e. Eq.[1]) (BSI, 1991). The flaw shape was assumed to vary from the most severe (straight-fronted, a/2c=0), to the less severe a/2c=0.5. The fatigue lives obtained by integrating the Paris law for a given stress range are compared with experimental results and relevant design values in Fig.10(b). The agreement between the mean and upper limit is seen to be very good, but the lower limit was seriously underestimated on the basis of the most conservative assumptions. In fact, fracture mechanics predicts a lower life than the design value. This suggests that the use of fracture mechanics in conjunction with the most conservative assumptions about input parameters would be too conservative. Some relaxation is possible if account is taken of the general observation from fatigue tests that the assumption of $(a/2c)_i$=0 is too severe, in that even assuming a small initial value results in much longer predicted lives. For example, for $(a/2c)_i$=0.05, the life is 70% higher, while the actual lower limit to the fatigue data is predicted if $(a/2c)_i$=0.5. Thus, accepting that a conservative estimate of the initial flaw size is a_i=0.4 and $(a/2c)_i$>0, it is clear that fracture mechanics calculations based on actual crack growth data for the particular steel being considered could predict longer fatigue lives than the design S-N curve and hence be less conservative. Figure 10(b) includes the example of steel with C corresponding to the published mean value and $(a/2c)_i$=0.5.

An additional advantage of direct use of fracture mechanics is the ability to model crack growth under variable amplitude loading more accurately, perhaps including stress history effects and certainly treating stresses below the constant amplitude fatigue limit more precisely using the threshold stress intensity factor. However, advantages of the approach have to be set against the time, expense and inconvenience involved in obtaining the properties of the actual material to be used in the structure being designed.



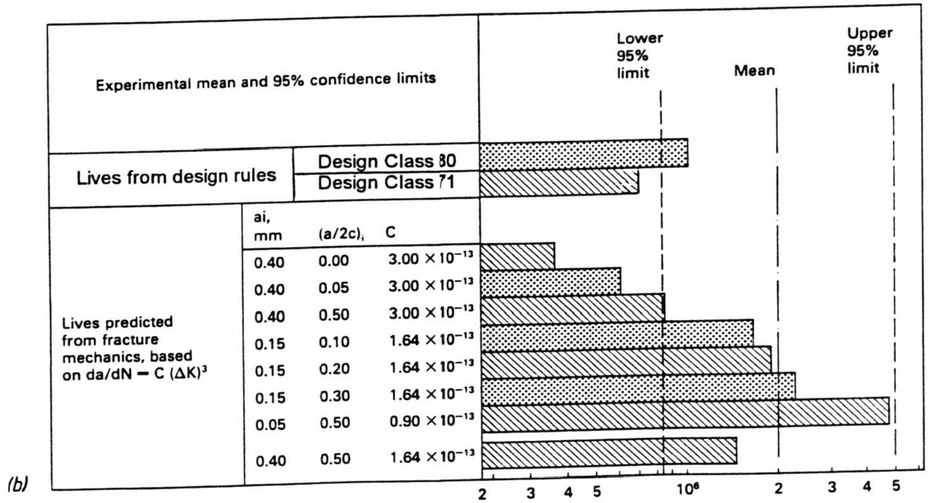


Fig.10 Examples of fracture mechanics fatigue life predictions:

a) Actual fatigue data;
b) Comparison of actual lives, design values and predicted lives for a stress range of 100MPa.

CONCLUDING REMARKS

There is no doubt that fracture mechanics has played an important part in the development of fatigue design rules for weldments, both in terms of understanding the factors which affect fatigue life and the form of rules. Important areas for future research are:

- Better understanding of fatigue under variable amplitude loading and improved ability to predict fatigue lives.
- Clarification of the fatigue damage due to stresses below the constant amplitude fatigue limit.
- Simplification of fatigue rules based on further clarification of the influence of welded joint geometry and size.
- Design data for welding processes or methods expected to give improved fatigue performance.
- More accurate K solutions relevant to cracks in weldments and validation of life prediction methods by reference to experimental data.

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