The use of a non-conventional fracture mechanics parameter for estimating SN curves of welded joints

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Abstract

The notch stress intensity factor (NSIF) is an extension of the stress intensity factor (SIF) concept for pointed V-notches which allows the stress field intensity in uncracked geometries to be known. A fatigue crack departing from the weld toe, for example, will be under the influence of the NSIF as long as the weld toe is modeled as a pointed V-notch. If the path and shape of this crack are considered constant, then their driving force, the SIF, can be linked to the NSIF and a valid fatigue crack growth (FCG) analysis can be performed in both the short and long crack regimes. In the present paper, the previsions thus obtained were compared with fatigue strength data taken from the literature and highly conservative results were found. These results, however, confirm that the weld toe in the type of joints studied can be modeled as a pointed V-notch with a definite opening angle and the stress field in the small region near the notch tip can be completely described by the NSIF. The difficulties related to the use of the classical linear elastic fracture mechanics (LEFM) in the short crack regime could be overcome. Also, the LEFM-NSIF-based procedure was capable of reducing the width of the scatter band in SN curves when comparing to the nominal stress concept. On the other hand, the approach was tested for various initial crack sizes and also for another set of Paris’ constants and an undesirable high sensitivity to these parameters was found. Its use in real-world engineering problems is limited and it must always be taken into account that the results are strongly dependent on material parameters and initial values.

Keywords: fatigue of welded joints, fatigue crack growth, notch stress intensity factor

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1. INTRODUCTION

The fatigue design of welded joints is normally obtained through the nominal stress concept (NSC). In this approach, the range of the nominal stress $\Delta \sigma_n$ is compared with its permissible value (strength) in accordance with the similarity principle. The traditional stress concentration factor $K_t$ is meaningless for welds since the fatigue strength is related to the nominal stress distribution defined not considering the stress-raising effects of the weld geometry itself. The fatigue resistance curves (SN) are classified by defining a number of fatigue qualities which depend on the structural details of the welded joints. For standard applications, e.g., the International Institute of Welding presents a single graph where different SN curves correspond to several FAT (Fatigue Design Classes) values. Figure 1 shows an example of this classification. The number following the FAT stands for the allowable nominal stress range (in MPa) at $2 \times 10^6$ cycles [Hobbacher (2007)]. Larger FAT values are associated with better fatigue properties.

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| Nomenclature                  | Description                                      |
|-------------------------------|--------------------------------------------------|
| $a$                           | Crack size                                       |
| $a_0$                         | El Haddad’s length parameter                     |
| $C_p, m_p$                    | Material’s parameters corresponding to a Paris’ type relation |
| CS                            | Coordinate system                               |
| $da/dN$                       | Fatigue crack growth rate                        |
| FAT                           | Fatigue design classes                           |
| FCG                           | Fatigue crack growth                             |
| FEA                           | Finite element analysis                          |
| $K$ or SIF                    | Stress intensity factor                           |
| $k$                           | Non-dimensional parameter                        |
| $K^r$ or NSIF                 | Notch stress intensity factor                     |
| LEFM                          | Linear elastic fracture mechanics                 |
| NSC                           | Nominal stress concept                            |
| $r, \theta$                  | Polar coordinates                                |
| R-ratio                      | Load relation=min/max                             |
| SN                            | Strength versus number of cycles in fatigue tests |
| $t, L$ and $h$                | Geometrical dimensions of the plate and the weld  |
| $\alpha$                     | Notch opening angle                               |
| $\rho$                       | Notch tip radius                                 |
| $\lambda$, $\chi$           | Eigenvalues of the William’s solution            |
| $\sigma$, $\tau$             | Normal and shear stresses, respectively           |

A distinctive, but odd, feature of the NSC for welded joints, when compared with other structural fatigue design approaches, is the independence of the fatigue resistance curves on a second parameter such as mean load (or R-ratio= min. load/max. load). The SN curves are also independent of the type of material (they are applicable to any carbon or C-Mn steel with tensile strength below 700 MPa) and type of electrode.

In dealing with the design of welded joints against fatigue, it has been argued [Schijve (2012)] that defects such as undercuts or inclusions, mainly in the weld toe, are always present and that they can act as initial cracks. The fatigue life of welded components would be mainly composed by the propagation phase, independently of the load level. The assumption of this initial damage suggests the use of a FCG analysis in order to keep the structural safety through periodic inspections. Indeed, linear elastic fracture mechanics (LEFM) approaches have been reliably used for modeling the behavior of fatigue toe cracks [Otegui et al. (1991)]. The British Standard Published Document (PD 6493, 1991), recently revised and renamed as BS 7910), has also incorporated LEFM procedures.
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SIF is a stress-field parameter which characterizes the stress/strain levels around the crack tip. The stress distribution slopes in the crack front according to the classical SIF are \(-0.5\) and their validity is restricted to the dominated singularity zone. SIF includes, in its definition, the crack size, the nominal stress and the geometrical proportions of the cracked body. The definition of the SIF solution for the particular welded joint geometry under consideration is one of the major drawbacks in the application of LEFM-based procedures. The LEFM approach is based on the integration of the relationship between the \(\frac{da}{dn}\) rate of fatigue crack growth (FCG) and the SIF range \(\Delta K\). Both an assumed initial crack-like defect and a final crack size should be sized. Then, for each value of nominal stress, a number of cycles can be calculated after the integration process. With these two parameters in hand, a classical SN curve can be predicted. Two main problems arise when using this method: 1) short cracks, mainly those that are departing from notches, are not well modeled by the \(\frac{da}{dn}\) vs. \(\Delta K\) relationship (due to plasticity effects) and 2) residual life calculations show a high sensibility to the initial crack size.

In any case, it is the highly stressed zone near the notches that controls the nucleation of small cracks and the growth of long cracks. Stress distributions for pointed V-shaped notches (as weld profiles are commonly modeled) can be obtained analytically [Williams (1952)] or using numerical techniques (as the finite element method FEM). Slopes in these cases are different from those provided by LEFM. These stress distributions were used as a significant fatigue parameter for comparison with fatigue strength of welded joints [Atzori (1985)]. No explicit definition of local stress parameter was made at that time. In the seventies [Mendelson & Gross (1972)] a stress-field parameter, the notch stress intensity factor (NSIF), was proposed to describe the amplitudes of stress fields according to William’s solution. As an extension of the SIF concept to notches, NSIF describes stress fields in uncracked geometries.

Atzori et. al (2008) have used a simplified procedure proposed in Albretch & Yamada (1977) for calculating the SIF of cracks under the influence of local tensile stresses. If these cracks are growing by fatigue in the region near
the weld toe (as usually is the case with welded joints), the local stresses governing the propagation are functions of NSIF. On this basis, residual life calculations can be made and a LEFM-based SN curve can be obtained. This explicit link between the traditional SIF and the so-called non-conventional fracture mechanics parameter NSIF will be explored in the present paper.

2. The Notch Stress Intensity Factor Approach

In welded joints, the region of the weld toe has a finite but small radius. A typical weld geometry can be modeled as a sharp V-notch with tip radius $\rho=0$. Fig. 2 shows a local polar coordinate system (CS) for stress analysis. The origin of the CS is coincident with the notch tip and the angle $\theta$ is measured from the notch bi-sector in a counterclockwise sense. The linear elastic stress fields can always be considered as the sum of symmetric (mode I) and skew-symmetric (mode II) fields. As in the classical LEFM, these stress fields are completely defined by their respective mode I and II NSIFs ($K_{1N}$ and $K_{2N}$) as shown by Williams’ solution [Lazzarin & Tovo (1996)]:

$$\left\{ \begin{array}{c}
\sigma_{\theta} \\
\sigma_r \\
\tau_{r\theta}
\end{array} \right\} = \frac{1}{\sqrt{2\pi}} \frac{r^{\lambda_i-1} K_{iN}}{(1 \pm \lambda_i) + \chi_i(1 \mp \lambda_i)} f(\lambda_i, \chi_i, \theta)$$

(1)

The parameters $\lambda_i$ and $\chi_i$ in Eq. (1), where $i=1$ or 2 for mode I and II, respectively, are the eigenvalues of William’s solution. These are only functions of the V-notch angle (note the parameter in Fig. 2 $q=(2\pi-2\alpha)/\pi$). For the most common value of $2\alpha=3\pi/4=135^\circ \lambda_{1,2}=(0.674,1.302)$ and $\chi_{1,2}=(4.153,-0.569)$, and the symmetric part of the solution shows the typical singularity of the elastic stresses at the notch tip. When dealing with the scale effect, Lazzarin and Tovo (1998) demonstrated that the range of NISF for both loading modes could be related to the nominal stress $\Delta\sigma_n$ as follows:

$$\Delta K_{IN} = k_i \Delta \sigma_n t^{1-\lambda_i}$$

(2)

where “$t$” is the main plate thickness (the width ahead of the notch) and “$k$” is a non-dimensional parameter similar to the well-known stress concentration factor and depends on the welding joint geometry and loading conditions. It should be noted that even for mode II the reference stress is also the nominal tensile stress “$\Delta\sigma_n$” and not the nominal shear stress “$\Delta\tau_n$”. According to Eq. (1) along the notch bi-sector ($\theta=0^\circ$), the two loading modes are uncoupled, which means that radial and circumferential stresses are related to $K_{1N}$ while the shear stress just depends on the sliding mode or $K_{2N}$. Lazzarin and Tovo (1998) took advantage of this fact and determined the coefficients $k_1$ and $k_2$ for various weld-like geometries by means of FE analysis. The best fit to numerical results of around 25 different geometries returned the following expressions:

$$k_1 = 1.212 + 0.495 \cdot e^{-0.985(2h/t)} - 1.259 \cdot e^{-1.120(2h/t)-0.485(L/t)}$$

$$k_2 = 0.508 - 0.797 \cdot e^{-1.9592(2h/t)} + 2.723 \cdot e^{-1.126(2h/t)-0.769(L/t)}$$

(3)

where $L$ and $h$ are geometrical dimensions of the transverse plate and the weld, respectively. The $\Delta K_{IN}$ was able of summarizing a large body of experimental data on steel and aluminum welded joints which main plate thickness ranges between 6 and 100 mm (steel) and between 3 and 24 mm (aluminium).
3. Correlation between the NSIF and the LEFM stress intensity factor \( K \)

Atzori et al. (1996) noted that the SIF of a crack propagating in a zone affected by a stress gradient could be estimated with a simplified procedure proposed by Albrecht & Yamada in 1977. The procedure is a direct consequence of the superposition principle. The advantage of this approach consists in that \( K(a) \), where “a” is the crack size, can be defined on the basis of a linear elastic analysis of the uncracked member. As can be seen below (Eq. (4)), the solution depends on the circumferential \( \sigma_\theta(r) \) which, in turn, is supplied by William’s solution, Eq.(1):

\[
K_I = F \sqrt{\pi a} \frac{2}{\pi} \int_0^a \frac{\sigma_\theta}{\sqrt{a^2 - r^2}} dr
\]

Note that in order to explicitly define the link between NSIF and LEFM, the propagation of a fatigue crack should be due to the stress acting perpendicularly to its faces, in other words, \( \sigma_\theta \). Also the crack path is simplified as a straight line and with a direction normal to the main plate surface (\( \theta=22.5^\circ \)) and therefore departing from the weld toe. In such case, the mode I and II contributions should be added, resulting in the following total circumferential stress and mode I SIF of a propagating crack, respectively:

\[
\sigma_\theta = \frac{0.36}{r^{0.326}} K_1^N + 0.32 r^{0.302} K_2^N
\]

\[
K_I = F \sqrt{\pi a} \left( \frac{0.43 K_1^N}{a^{0.326}} + 0.24 a^{0.302} K_2^N \right)
\]

where a constant weld angle of 2\( \alpha=135^\circ \) was assumed (then \( \lambda_{1,2}=0.674,1.302 \) and \( \chi_{1,2}=4.153,-0.569 \)). Note that the SIF corresponds to mode I despite the fact that stress fields governed by NSIFs are the sum of both loading modes I and II. The dimensional analysis of Eq. (5) is consistent since \( K_1^N \) and \( K_2^N \) have units of MPa.m\(^{0.326} \) and MPa.m\(^{-0.302} \), respectively. Obviously, for a range of nominal stresses \( \Delta\sigma n \) acting on the main plate, a fatigue crack
would grow from the weld toe according to a $\Delta K$ driving force. A closed form for the $\Delta K$ of this crack can be obtained by substituting Eq. (2) into the Eq. (5):

$$\Delta K = F \cdot \sqrt{\pi a} \cdot \left(0.538 \cdot k_1 \cdot \left(\frac{t}{a}\right)^{0.326} + 0.239 \cdot k_2 \cdot \left(\frac{t}{a}\right)^{-0.302}\right) \cdot \Delta \sigma_n$$

(6)

As usual, the dependence on crack size should be confined to the term inside the square root and to the geometry factor “F”. Then Eq. (6) can be rewritten as:

$$\Delta K = F \cdot \sqrt{\pi a} \cdot \Delta \sigma_{g,i}$$

$$\frac{\Delta \sigma_{g,i}}{\Delta \sigma_n} = \left(0.538 \cdot k_1 \cdot \left(\frac{t}{a_0}\right)^{0.326} + 0.239 \cdot k_2 \cdot \left(\frac{t}{a_0}\right)^{-0.302}\right)$$

(7)

The new term $\Delta \sigma_{g,i}$ is the range of initial nominal gross stress, i.e. the nominal stress when the crack size is enough for being propagated. The initial value of the edge crack length “$a_0$” is the well-known length parameter that establishes a line between propagation and non-propagation conditions [Lukas et al. (1986)]. The $\Delta \sigma_{g,i}$ can be regarded as an effective stress that includes the effect of different thickness in fatigue strength of welded joints. Equation (7) can then be inserted in some FCG “law” (da/dN vs. $\Delta K$ relation) and the residual life of welded joints can be calculated.

4. Experimental Data

The experimental data used in this paper for comparison with LEFM previsions were published by Otegui et al. in 1991. They applied fatigue test to a series of four bead-on-plate and 14 full-penetration T-plate type non-load-carrying welded joints. The T-plate specimens were obtained by both manual and automatic welding procedures. In the latter case, the root pass was obtained by a GMAW process, while in the subsequent passes, the SAW process was used. In all cases, the base material was a low carbon steel. Table 1 summarizes the test parameters and fatigue results obtained by Otegui et al. (1991).

5. Residual life calculations

A general relation for the FCG resistance is $da/dN = f(\Delta K, R)$. The number of cycles $N_f$ between two crack sizes, an initial $a_i$ and a final one $a_f$, can be calculated by simply integrating this differential relation or:

$$\int_{N_i}^{N_f} dN = N_f - N_i = \int_{a_i}^{a_f} \frac{da}{f(\Delta K, R)}$$

(8)

The range of SIF from Eq. (5) and some material parameters determined in FCG tests can be used for integrating Eq. (8). If data from various combinations of $\Delta K$ and $R$ are available, these parameters can be obtained through a multiple linear regression analysis. Other approaches disregard the $R$-ratio influence and use, conservatively a Paris’ type power relation $da/dN = C_p \cdot \Delta K^{m_p}$ where only FCG rate $da/dN$ and $\Delta K$ data are needed for obtaining the “$C_p$” and “$m_p$” parameters. In this paper, only the second group is addressed for residual life estimates since, as mentioned above, the FCG resistance of welded joints is believed to be $R$-ratio insensible. Generally, steels with good
weldability have from moderate to low hardening exponent and this explains the low susceptibility of welded joints to mean load effects [Castro & Meggiolaro (2009)].

Table 1 – Test parameters and fatigue results of experimental data [Otegui et al. (1991)] used for comparison in the present work.

| Main Thickness Plate | Nominal Stress Range | Load ratio | Total Life, x10⁶ cycles |
|----------------------|----------------------|------------|------------------------|
| t, mm                | Δσ, MPa                       | R          |                         |
| 25                   | 215 0.1 2.0                |            |                         |
| 235 0.2 0.8          |                            |            |                         |
| 235 0.2 1.98         |                            |            |                         |
| 235 0.2 1.28         |                            |            |                         |
| 300 0.2 0.9          |                            |            |                         |
| 305 0.1 0.6          |                            |            |                         |
| 305 0.1 0.6          |                            |            |                         |
| 250 0.1 1.13         |                            |            |                         |
| 165 0.1 4.05         |                            |            |                         |
| 400 0.1 0.25         |                            |            |                         |
| 250 0.1 0.63         |                            |            |                         |
| 252 0.62             |                            |            |                         |
| 174 0.1 1.65         |                            |            |                         |
| 176 0.73             |                            |            |                         |
| 175 0.65             |                            |            |                         |
| 175 2.7              |                            |            |                         |
| 174 0.1 1.6          |                            |            |                         |
| 172 0.66             |                            |            |                         |

After substituting Eq. (6) in Paris’ form, i.e. $da/dN=C_p \Delta k^{mp}$ into the Eq. (8) and integrating, the following closed form for $F(a)=constant$ and $mp \neq 2$ can be obtained:

$$N = \frac{a_f - a_0}{2} \cdot \frac{mp}{2} \cdot C_p \cdot \left( F \cdot \sqrt{\pi} \cdot \Delta \sigma_{g,i} \right)^{mp} \cdot \left( 1 - \frac{m_p}{2} \right)$$

(9)

When the expression for $\Delta \sigma_{g,i}$ (Eq. (7)) is inserted into the Eq. (9), the number of cycles predicted by the LEFM-based approach described in this paper becomes dependent on the following parameters:

$$N = N \left( t, \Delta \sigma_n, a_0, a_f, k_1, k_2, C_p, m_p, F \right)$$

(10)

The thickness “t” and the range of nominal stress “$\Delta \sigma_n$” are taken from the experimental data (Table 1) while the initial and final crack sizes were, according to suggestions of Atzori et al. (2008), $a_0=0.3$ mm and $a_f=t/3$, respectively. Both choices will be discussed in more details later in the paper. The parameters $k_1$ and $k_2$ from Eq. (6) are obtained after substituting the geometrical proportions of welded joints from experimental data in Eq (3). As two different geometries, there is a $k_{1,2}$ for each one of them. The two set of Paris’ constants (Cp and mp) used [Gurney (1991)] are shown in table 2. The growing crack was considered as a semi-elliptical surface crack with its major axis “2c” much longer than the minor axis “a”. Then the geometry factor can be considered constant (F=1.122). The
predicted lives calculated by Eq. (9) for each $\Delta \sigma_n$ in table 1 are compared with the experimental ones in $\Delta \sigma_{g,i}$ vs. $N$ coordinates in fig. 3.

Table 2 – The two set of Paris’ constants [Gurney (1991)] used for calculating the residual life.

| Set | $C_p$ m/cycle(MPa.m$^{1/2}$) | $m_p$ |
|-----|-----------------------------|-------|
| 1   | 0.183E−12                  | 3     |
| 2   | 0.204E−15                  | 4     |

Fig. 3 – Comparison between the experimental total life according to table 1 and residual life calculations for Eq. (9) for two sets of Paris’ constants (table 2). All fatigue failures in experimental data were originated at the weld toe.

6. Discussion

The FCG calculations based on the $\Delta K(\text{NSIF},a)$ from Eq. (5) impose a range of applicability to this analysis which is the same of the linear stress field governed by NSIF. The NSIF is valid within $t/3$ from the notch tip, i.e. the crack tip cannot be situated at a distance of more than $t/3$ from the notch tip so that the FCG analysis based on NSIF is correct (11). This explains why the final crack size or upper limit of the integral in Eq. (9) was $a_f=t/3$. Also, cracks larger than $t/3$ become unstable in fatigue and their FCG analysis makes no sense [Atzori et al. (2008)]. Anyway, the final crack size does not have a great influence in the residual life calculations, as can be seen in fig. 4. In this case, a simulation keeping first $a_i$ constant and varying $a_f$, and later, keeping $a_f$ constant and varying $a_i$, was made for the experimental series used in this paper. The first set of Paris’ constants (table 2) was used for this comparison.

The fatigue limit of welded joints is conventionally reported as the nominal stress range that does not initiate fatigue cracks after $2 \times 10^6$ loading cycles. It is well known, however, that many short cracks could have been born and grown after a few loading cycles. Indeed, the fatigue limit can be defined as the lowest stress range for which crack nucleation is followed by crack growth until failure [Schijve (2009)]. As mentioned earlier, for $\theta=0^\circ$ the two loading modes are uncoupled and the driving force of such cracks can be calculated as:

$$
\Delta K_f = F \cdot \sqrt{\pi a} \cdot \left( 0.59 \cdot k_1 \cdot \left( \frac{t}{a} \right)^{0.326} \right) \cdot \Delta \sigma_n
$$

(11)
Fig. 4 – The final crack size has little influence on the residual life calculations (a) while the selection of the initial crack size causes differences of orders of magnitude in this parameter (b). Similar trends are also expected for any FCG analysis since the FCG rate dN/da decreases exponentially as the crack grows.

By imposing the limiting condition that $\Delta K_i = \Delta K_{th}$ and $\Delta \sigma_n = \Delta \sigma_o$ in Eq. (11), where $\Delta K_{th}$ and $\Delta \sigma_o$ are the thresholds for FCG and high cycle fatigue HCF, respectively, the size of cracks that are not able to propagate can be found. This has been done by Atzori et. al. (1998) for a group of fillet welds and the mean value obtained was 0.27 mm. This crack size can be considered equivalent (in order of magnitude) to the short crack size correction “$a_0$” of El Haddad et. al (1979) for separating the short crack and the long crack regimes. It is worth noting that $a_0$ is not a material property but, rather, a material parameter which depends on the particular geometry and R-ratio. Anyway, an exact separation between the initiation and the propagation process is impossible and the initial crack size $a_i=0.3$ mm adopted here should be seen as an engineering approximation. Different from the final crack size, $a_i$ has a greater influence in the calculated number of cycles to failure as shown in fig. 4.

The residual life calculations were made considering $\Delta K$ as the only variable involved in the FCG process, i.e. no approaches for dealing with mean load effects were used. Only the Paris’ law was enough for doing the previsions. In fact, FCG curves for welded joints should be measured in specimens of real size so as to prevent the diminution of the weld metal constraint and consequently the release of the (high) residual stresses [Castro & Meggiolaro (2009)] that are always present. The residual stresses are also responsible for the independence from R-ratio. They displace the mean load to a level where small and long cracks are always opened and no R-effect is observed.

The residual life estimates are well below the experimental data, mainly for the first set of Paris’ constants (fig. 3). However, and in spite of the fact that the amount of experimental data is limited, the LEFM-based procedure greatly reduced the scatter of points. This means that the parameter $\Delta \sigma_{gi}$ was able to unify the fatigue strength of welded joints of different geometries and thicknesses. The mean values of the ratio of experimental total life to residual life are 51.8 and 5.23 for the first and second sets of material parameters, respectively. These values indicate that, under the conditions of the simulation made in the present work, the crack initiation life was an important fraction of the total life for tests under consideration. The so-called “short crack propagation life” can consume from 25 to 50% of the total life [Verreman & Nie (1996)]. At the end of this phase, the crack size is around 0.5 mm. From these observations and considering the initial crack size ($a_0=0.3$ mm) and geometry factor (F=1.122) used in this paper, the calculated residual life is expected to be the lowest possible percentage of the total life.

It is also interesting to note that for the first set of material parameters, the scatter band of two curves is almost parallel. This means that under both low and high stresses, the percentage of initiation life regarding total life is the same. Of course, low stresses take longer to the end of the initiation period but the proportion of the predicted residual life to experimental total life remains approximately the same. Similar results have been reported previously [Hou & Charng (1997)]. On the other hand, for the second set of material parameters, the relation between predicted and experimental life becomes stress-dependent. Despite the fact that the agreement has been improved, the strong
variation between two realistic set of Paris’ parameters conspire against the generality of the method. For both simulations, however, the capability of a LEFM-NSIF-based procedure should be highlighted for reducing the width of the scatter band in comparison to the nominal stress concept NSC.

7. Conclusions

The weld toe is one of the preferred sites where fatigue cracks initiate and propagate in welded joints. In this paper this highly stressed zone is described through NSIF. This parameter was originally developed and used for initiation but a link with FCG analysis has been successfully introduced recently. The symmetrical components of the stress fields described by NSIF are singular, i.e. $\sigma \propto 1/r$. However, using the superposition principle, there was no need for considering the notch radius different from zero to avoid the stress/strain singularities. The loading paths that contour the sharp V-notch at the weld toe are described by mode I and II stress intensity factors, or NSIFs. In this paper, cracks in the range $0.3 \text{ mm} < a < t/3$ were supposed to be under the influence of this loading paths, allowing their propagation behavior to be modeled by a link between NSIF and the classical LEFM. The total lives predicted using this approach were then compared with those of a selected group of experimental data and an encouraging conservative agreement was found. However, in addition to the well-known high sensibility of LEFM-based procedures to the initial crack size, the residual life as calculated in this paper has changed strongly with the so-called material properties (Paris’ curve constants). This is the main drawback of the approach and further developments should take it into account.

References

Albretch P, Yamada K, 1977. Rapid calculation of stress intensity factors, Journal of the Structural Division, ASCE 103, 377-389.
Atzori B, 1985. Notch effect or linear elastic fracture mechanics in fatigue design, In: Proceedings XIII AIAS Conference, Bergamo, Italy.
Atzori B, Lazzarin G, Meneghetti G, 2008. Fatigue strength assessment of welded joints: From the integration of Paris’ law to a synthesis based on the notch stress intensity factors of the uncracked geometries, Eng. Fracture Mechanics, 75, 364-378.
Atzori B, Lazzarin P, Tovo R, 1999. From a local stress approach to fracture mechanics: a comprehensive evaluation of the fatigue strength of welded joints, Fat Fract Engng Mat Struct, 22, 369-381.
Atzori B, Lazzarin P, Tovo R, 1999. Stress field parameters to predict the fatigue strength of notched components, The Journal of Strain Analysis for Engineering Design 34: 437-453.
British Standard Published Document, PD 6493, 1991. Guidance on methods for assessing the acceptability of flaws in fusion welded structures, BSI Standards, London.
Castro JTP, Meggiolaro MA, 2009. Fadiga: Técnicas e Práticas de Dimensionamento Estrutural sob Cargas Reais de Serviço, Volumes I e II, ISBN 978-1449514709, CreateSpace.
El Haddad MH, Topper TH, Smith KN, 1979. Prediction of non-propagating cracks, Engineering Fracture Mechanics, 11, 573–84.
Gurney TR, 1991. The fatigue strength of transverse fillet welded joints, Cambridge: Abington Publishing.
Hobbacher A, editor, 2007. Recommendations for fatigue design of welded joints and components, IIW Doc XIII-215r1-07/XV-1254r1-07, Inst. Int. Welding.
Hou CY, Chang JJ, 1997. Models for the estimation of weldment fatigue crack initiation life, Int. J. Fatigue 19, 537-541.
Lazzarin P, Tovo R, 1996. A unified approach to the evaluation of linear elastic stress fields in the neighborhood of cracks and notches, Int. Journal of Fracture 78, 3-19.
Lazzarin P, Tovo R, 1998. A notch intensity factor approach to the stress analysis of welds, Fat Fract Engng Mat Struct, 21, 1089-103.
Lukas P, Kunz L, Weiss B, Stickler R, 1986. Non-damaging notches in fatigue, Fatigue Fract Engng Mater. Struct. 9, 195-204.
Mendelson A, Gross R, 1972. Plane elastostatic analysis of V-notched plates, Int. Journal of Fracture Mechanics, 8 p. 267-327.
Otegui JL, Burns DJ, Kerr HW et. al, 1991. Growth and Coalescence of Fatigue Cracks at Weld Toes in Steel, Int. Journal of Pressure Vessel and Piping, 48, 129-165.
Radaj D, Sonsino CM, Fricke W (2009). Recent developments in local concepts of fatigue assessment of welded joints, Int. Journal of Fatigue, 31, 2-11.
Schijve J, 2009. Fatigue of Structures and Materials, 2nd ed. Springer, Netherlands.
Schijve J, 2012. Fatigue predictions of welded joints and the effective notch stress concept, Intern. J. Fatigue 45, 31-38.
Verreman Y, Nie B, 1996. Early development of fatigue cracking at manual fillet weld, Fatigue Fract Engng Mater. Struct. 19, 669-681.
Williams ML, 1952. Stress singularities resulting from various boundary conditions in angular corners of plates in tension, Journ of Applied Mechanics, 19, p. 526-528.