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Total life approach for fatigue life estimation of welded structures

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Abstract

It was shown that estimation of fatigue lives of welded joints can be successfully carried out by considering the fatigue process as a fatigue crack growth from the initial intrinsic crack size of $a_0=\rho^*$ until the final crack a_f. Such an approach avoids a somewhat arbitrary division of the fatigue process into the crack initiation and propagation and concentrates on using only one methodology - the fracture mechanics theory. The stress intensity factors can be determined in such cases by the weight function method. The proposed methodology allows estimation of the fatigue life under both constant and variable amplitude loading.

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Keywords: Total life; fatigue crack growth; welded joints

1. Total life approach

An approach to estimating the entire fatigue life, from the very early stages to the final fracture, of smooth specimens subjected to constant amplitude loading histories and welded components subjected to both constant and variable amplitude loading histories is discussed in this paper. The proposed method uses only experimental strain-life and stress-strain data obtained from smooth material specimens and da/dN versus ΔK crack growth data obtained from compact tension specimens. The proposed model is based on analyzing the local stress-strain material behavior in the vicinity of a crack tip.

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Nomenclature

ρ^*	the smallest crack size in the material being within the resolution of continuum mechanics
$K_{max,appl}$	applied maximum stress intensity factor
ΔK_{appl}	applied stress intensity range
K_R	residual stress intensity factor due to welding process
K_r	residual stress intensity factor due to reversed plastic deformations
C,γ	fatigue crack growth material constants
p	driving force parameter
W	weight function
s(x)	stress distribution in the critical cross-section
a,b	dimensions of the semi-elliptical crack

1.1. Basic assumptions of the total life approach

The Total Life approach is based on four main assumptions:

- The fatigue crack is regarded as a deep notch with a finite tip radius, ρ^*. The ρ^* parameter is a material constant.
- The size of the smallest crack which can be analyzed using classical mechanics of continuum is equal to ρ^*.
- The stress-strain material behavior can be described by the cyclic Ramberg-Osgood [1] stress-strain curve.
- The number of cycles required to break the material over the distance ρ^* can be calculated using the Manson-Coffin [2] equation and the mean stress correction proposed [3] by Smith, Watson and Topper.

The advantage of using the blunt crack model lies in the fact that notch theories can be applied and crack tip stresses and strains obtained in the analysis are more realistic than in the case of a sharp crack leading to the singular solution. Such an approach implies that while crack surfaces may get in contact away from the crack tip, the region (Fig. 1) just behind the crack tip remains open. The blunt crack tip model makes it possible to carry out elastic plastic stress-strain analysis around the crack tip using simplified methods like the multiaxial Neuber rule [4]. One of the advantages of the model is consistent treatment of the tensile and compressive parts of the stress cycle. The compressive stress effect is modeled by converting the crack into a small hole of the radius ρ^* which is a material constant.

Based on the model shown in Fig. 1 and all the assumptions above, Noroozi and Glinka [5] have analytically derived the fatigue crack growth expression in the following form:

$$\frac{da}{dN} = C\left(\Delta K_{tot}^{\ 1-p} K_{\max,tot}^{\ 1-p}\right)^{\gamma} = C\left[\left(\Delta K_{appl} + K_r\left(\rho^*, S\right)\right)^{1-p}\left(K_{\max,appl} + K_r\left(\rho^*, S\right) + K_R\right)^{p}\right]^{\gamma} \tag{1}$$

Parameters '$K_{max,appl}$' and 'ΔK_{appl}' are the applied maximum SIF and the SIF range respectively, 'K_r' is the local residual SIF accounting for the effect of the crack tip residual stresses resulting from reversed plastic deformations, and K_R is the global residual SIF factor induced by the welding residual stresses. The SIF due to reversed plastic deformations is a function of the ρ^* parameter and it accounts also for the load interaction effects as discussed in reference [6]. Parameters C, γ, and p are found from the Manson-Coffin and Ramberg-Osgood material properties or from appropriate analysis [5] of fatigue crack growth data obtained at various R-ratios. Eq. (1) has been finally coded in the form of the UniGrow computer program [6] enabling fatigue crack growth analysis under a variety of loading spectra and geometrical crack configurations.

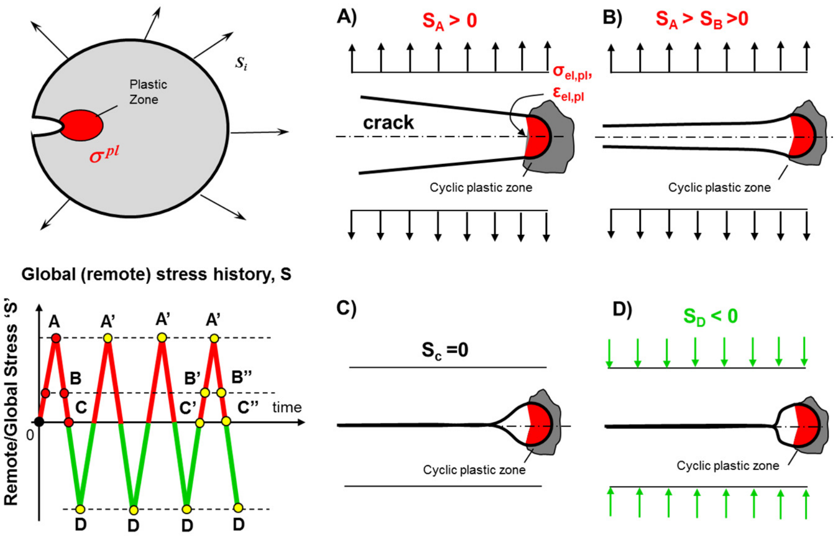

Fig. 1. Schematic of the UniGrow fatigue crack model based on the analysis of elastic-plastic strains ahead of the fatigue crack tip

*1.2. Estimation of the ρ*and other fatigue crack growth parameters*

The Total Fatigue Life approach requires the knowledge of fatigue crack growth material properties C, γ and ρ∗. The 'p' exponent present in expression (1) is a function of the strain hardening exponent n' and is defined in the literature [5]. If experimental fatigue crack growth data is not available these parameters can be easily obtained using equations proposed by Noroozi and Glinka [5]. However, in the case presented below, all the material properties needed (Ramberg-Osgood: K', n'; Manson-Coffin: σ_f', ε_f', b, c; Fatigue Crack Growth: C, γ) were available and obtained from a Society of Automotive Engineering Fatigue Design and Evaluation Committee (SAE FD&E) test effort. All that data had the same material microstructure, chemistry, hardness and standard material property values.

The Total Life approach assumes that there is no need to divide the fatigue process into the fatigue crack initiation and propagation stages and the entire fatigue life can be estimated using the fatigue crack growth approach and propagating a crack from its initial crack size of ρ* to the final fracture. If such an approach is feasible then it should be possible to predict the smooth specimen fatigue strain-life curve by using the constant amplitude fatigue crack growth data. This idea requires the estimate of a ρ* parameter which gives the best correlation between the experimental fatigue strain-life data obtained from testing smooth specimens and fatigue lives obtained from the UniGrow program [6]. That ρ* parameter will subsequently be used to demonstrate that it makes it possible to consolidate the constant amplitude fatigue crack growth data obtained under multiple R-stress ratios into one master curve. Discussed below is the detailed iterative procedure for obtaining the ρ*, C and γ parameters for the general fatigue crack growth expression (1).

Standard smooth specimens tested under fully reversed tension-compression cyclic loading with the stress ratio R= -1.0 are used to generate the material strain-life curve $\Delta\varepsilon/2$ - $2N_f$. The specimens diameter is usually D=6-8 mm. The initial crack size in the Total Life approach is assumed to be ρ*. Available experimental data indicates that the fatigue crack in a smooth specimen can be approximated by a straight front edge crack in a solid cylinder. The stress intensity factor for such a crack is given in reference [7]. Several small semi-elliptical cracks may simultaneously initiate at the surface of the specimen, but they quickly join each other forming a crack similar to that one shown in Fig. 2(a).

The initial estimate of the ρ* parameter necessary for the iterative procedure resulting in the best estimation of the strain-life fatigue data, can be assumed to be equal to the material grain size (the average grain size for this A36 steel was observed to be about 0.045mm. The value of the starting ρ* parameter is not very important since it is used only to begin the iterative process.

Starting with that initial value of ρ^*, the C and γ parameters of Eq. (1) are determined by collapsing, for the assumed ρ^* parameter, the available set of constant amplitude fatigue crack growth data for R ratios = 0.1, 0.2, and 0.3. The material fatigue crack growth constants C and γ are then determined by using a least square data fitting method. Fig. 2(b) shows the da/dN data presented as a function of the driving force after the final iteration. It can be seen, that the experimental data for three different constant amplitude R-ratios have collapsed into one master curve which shows the ability of the Total life approach to account for various mean stress levels.

The UniGrow fatigue life calculations were then performed for each experimental point of the $\Delta\varepsilon/2$ - $2N_f$ curve by using Eq. (1) and the appropriate stress intensity factor [7] values. This was possible since the initial crack geometry, the fatigue crack growth parameters of Eq.(1) and the ρ^* parameter have been already defined. The final crack size was assumed to be the crack size at the moment of the test termination, i.e. around half of the specimen diameter. The best correlation between the experimental and predicted fatigue life data (for the smooth specimens) was obtained with ρ^*=0.07mm. The calculated strain-life data points were finally plotted in the form of the solid line shown in Fig. 2(a) (note that is a plot of $\Delta\varepsilon/2$ - N_f). It should be pointed out that only the lower part of the calculated $\Delta\varepsilon/2$ - $2N_f$ curve is valid because it was obtained for nominal stresses less than the material yield limit. In the case of the upper part of the data set the nominal stresses in the analysed specimens were greater than the yield limit. It is surprising that even in the case of nominal stresses exceeding the yield limit the fatigue life estimations were not very far from the experimental values.

The procedure described above allows the determination of the fatigue crack growth material constants and the ρ^* parameter. These parameters can be subsequently used to estimate the fatigue life of more complex structures subjected to both constant and variable amplitude loading histories.

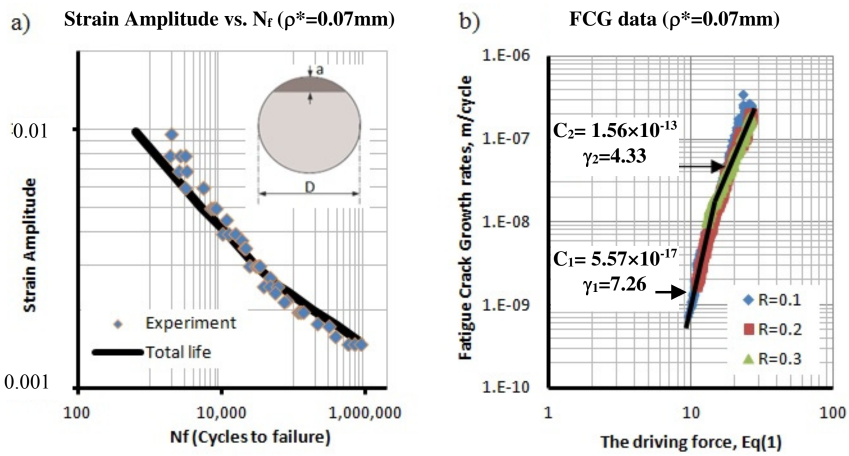

Fig. 2. (a) Strain-life exp. data and estimated curve, (b) FCG exp. data and estimated curve

2. Application of the Total Life methodology to A36 steel welded components

In order to validate the total life approach to address “real world” engineering problems it was decided to apply the total life analysis to welded T-joint components made of the A36 steel material. All necessary fatigue tests were carried out under the guidance of the Society of Automotive Engineering Fatigue Design and Evaluation Committee (SAE FD&E).

2.1. Fatigue analysis of welded specimens

Welded T-joint specimens made of A36 steel were tested in order to verify the validity of the Total Life approach. Fig. 3 shows one of the welded specimens used in the experiments. The thickness of the vertical welded plate was 25.4 mm and its width was 101.6 mm. The height of the horizontal base welded plate was 50.8 mm, its width was

101.6 mm and its length was 101.6 mm. All specimens were tested in bending created by a horizontal load applied to the T-joint component 225.25 mm vertically above the weld toe. The toe of the weld critical section cross section dimensions were 25.4 mm by 101.6mm. The T-joint specimens were tested at a 24 kN (maximum load) to constant amplitude loading cycles with R ratios = 0.1, 0.3, 0.5. They were also tested to a variable amplitude block cycle sequence described later.

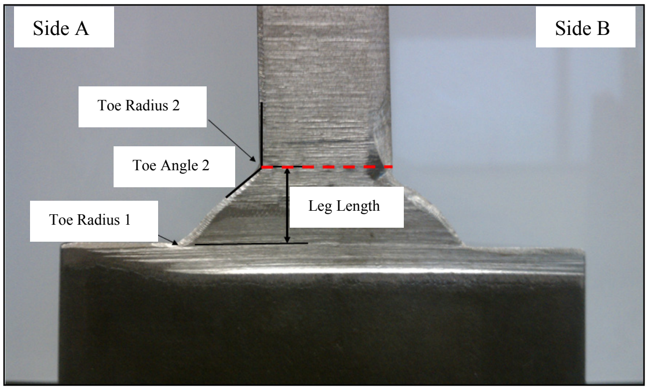

Fig. 3. Welded specimen made of A36 steel

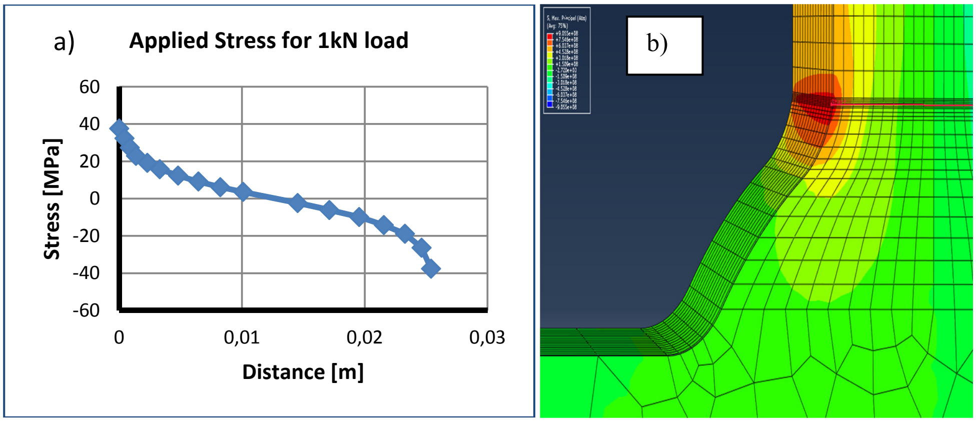

Fig. 4. (a) Through thickness stress field in the middle of the critical cross section, (b) Finite Element model

In order to carry out the analysis the stress distribution (Fig. 4(a)) in the critical cross section area was determined using the Finite Element method. The one-dimensional stress field shown in Fig. 4(a) was obtained at the width centre of the vertical attachment. This stress distribution made it possible to determine the applied stress intensity factor by the means of the weight function technique.

$$K_i = L\int_0^{a_i} \sigma(x)\cdot W(a_i, b_i, x)dx \quad (2)$$

Where: L is the applied load; σ(x) is the applied stress field shown in Fig. 4(a), and W is the weight function for a semi-elliptical crack in a finite thickness plate.

Based on the experimental observations the initial crack at the weld toe was assumed to be semi-circular with the radius equal to ρ*=0.07mm. The Total Life approach calculations were performed on a cycle by cycle basis and the varying crack geometry, as the crack progressed through the thickness, was analytically determined.

The analysed and tested welded specimens were not stress relieved. Therefore the effect of the residual stress needed to be taken into account. Therefore the x-ray diffraction technique was used by experts participating in the SAE FD&E Committee effort to measure residual stresses in never loaded specimens and to construct the through thickness residual stress distribution shown in Fig 5(a). It can be observed that the measured residual stress data was inconsistent near the plate surface showing erratic fluctuations. The measured residual stress field was not in equilibrium either. Therefore it was decided to slightly modify the measured residual stress field in order to satisfy the equilibrium and to smooth the entire residual stress field. The modified residual stress field is shown in Fig. 5(b).

The fatigue life analysis based on the approach described previously was carried out for original, modified and no residual stress distributions to better understand the sensitivity of the Total Life analysis to the residual stress field.

Knowledge of the residual stress distribution induced by the welding process makes it possible to calculate the residual stress intensity factor K_R by using the same weight function Eq. (2) as in the case of the stress intensity factor induced by the applied load. The only difference is that the applied stress distribution needs to be replaced by the residual stress field.

The same fatigue crack growth properties C, γ and ρ^* were used as in the case of the smooth specimen analyses. For each cycle of the loading history, the maximum applied stress intensity factor, the stress intensity range, and the residual stress intensity factor due to welding stresses were calculated. After applying the loading and unloading reversals the plasticity induced residual stress intensity factor K_r needed to be determined as well. The method to determine the K_r stress intensity factor responsible for the retardation and acceleration phenomena caused by variable amplitude loading is described in reference [6]. All necessary stress intensity factor estimations (Eq. (1)) and crack geometry updates were carried out after each applied loading cycle.

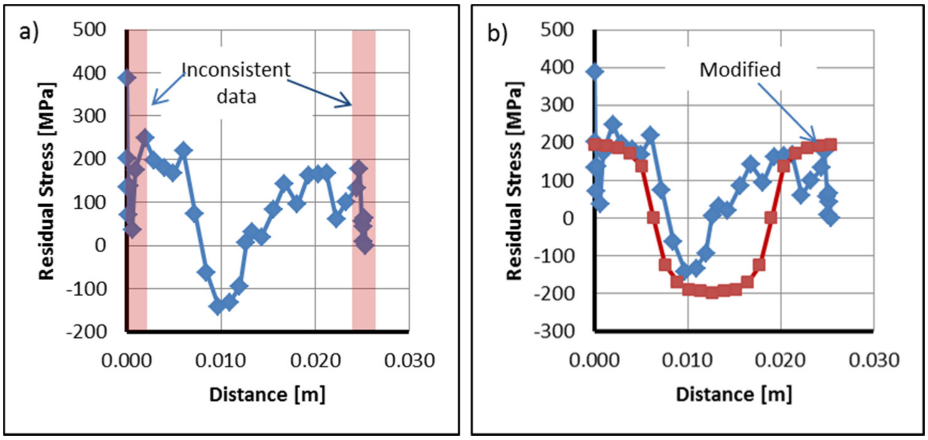

Fig. 5. (a) Measured residual stress field, (b) modified residual stress field

The fatigue life analyses and the experiments were carried out under constant amplitude loading for three R ratios, i.e. R=0.1, 0.3, 0.5. The maximum and minimum cyclic load levels for the analysed loading histories were L_{max}=24kN - L_{min}=2.4kN, L_{max}=24kN - L_{min}=8kN and L_{max}=24kN - L_{min}=12kN.

The experimental and calculated fatigue lives are shown in Fig. 6. The solid lines in Fig. 6 denote fatigue lives estimated by using the modified residual stress field. The dashed lines correspond to calculated fatigue lives based on the originally measured residual stress field. The dotted lines show calculated fatigue lives neglecting the presence of welding residual stresses. The predicted fatigue lives are close but slightly conservative in the case of the high stress levels and slightly non-conservative in the case of the low stress levels. However, if no residual stresses are accounted for in the analysis the predicted fatigue lives are very non-conservative.

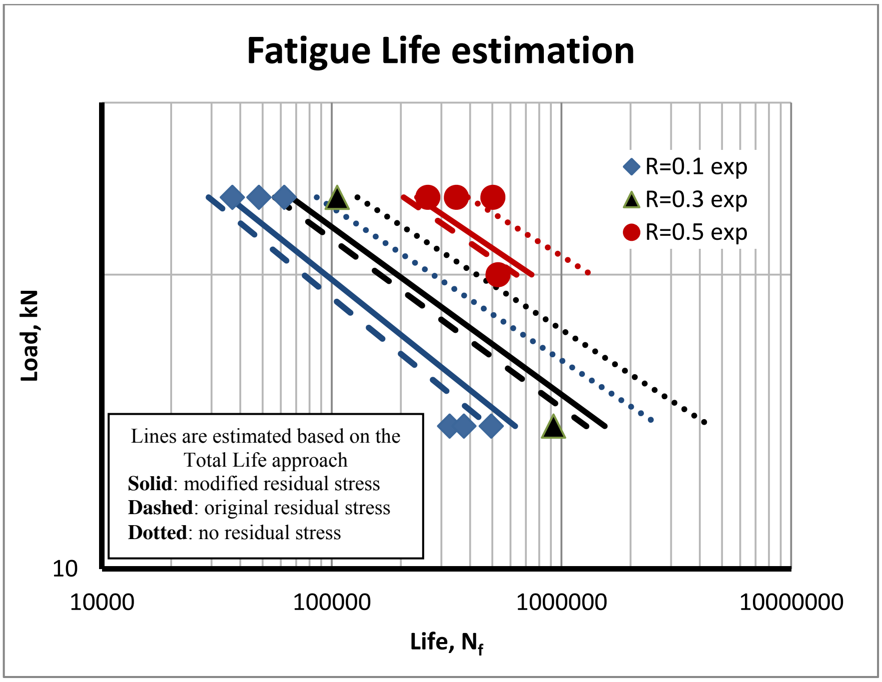

Fig. 6. Fatigue lives estimated by the total life approach and the experimental data

Additional validation was performed for the variable amplitude block loading. The loading block used in the analysis is a combination of two constant amplitude loading histories. The first 5,000 cycles in the block loading were applied with the maximum load of L_{max}=24kN and the minimum load of L_{min}=2.4kN. The second step of 40,000 cycles was applied with the same maximum load of L_{max}=24kN and the minimum load of L_{min}=12kN. The combined loading block of 45,000 cycles was repeated until the final failure. In this case the calculated fatigue lives estimated for both the original and the modified residual stress fields were close to each other and to the lower experimental life as well: The experimental fatigue lives to fracture were L_{exp} = 138,421 cycles and 174,069 cycles and the estimated fatigue lives were L_1 = 135,670 cycles for the originally measured residual stress field, L_2 = 136,979 cycles obtained for the modified residual stress field, and L_3=258,000 cycles fatigue life was determined while neglecting the presence of residual stresses. Unfortunately the crack growth was not monitored and only the final crack size was known at the end of the experiment.

3. Conclusions and recommendations

It has been demonstrated that the proposed Total Life approach when applied to the welded A-36 steel T-Joint configuration yielded a reasonable estimation of the total fatigue life of the component. This was accomplished by using only the Total Life fatigue crack growth analysis technique for both constant and variable amplitude loading histories. Such an analysis enables estimating the entire fatigue life of a component without the necessity of using an arbitrary division of the fatigue process into the crack initiation and propagation stages and it enables using only one methodology, i.e., the fracture mechanics based fatigue crack growth analysis. It has also been demonstrated that the Total Life fatigue crack growth method can be used to estimate fatigue lives of smooth specimens considered traditionally as being free, in an engineering sense, of initial cracks.

The analysis of T-joint specimens mentioned above was performed for two residual stress distributions: the original as measured stress distribution and the modified equilibrated residual stress field. The final results showed that modification of the residual stress field contributed only to a small difference between predicted fatigue lives. Since the experimental investigation of the welding residual stress field might be quite costly and time consuming, it is recommended that a standard of residual stress fields for different weld geometries might be established. If successful such an approach could help to generate appropriate residual stress distributions and easily include them into daily fatigue life analyses.

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