FAILURE CRITERIA OF WELDS IN MIXED MODE LOADING

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Abstract

Models of mild and low alloy, single and multi run welds have been proposed. A calculation of the fracture toughness K_c of welds, depending on their microstructure and non-metallic inclusions, was carried out making use of the Barenblatt's formula. The critical energy of decohesion G_c /elastic energy release rate/ was determined from the fracture toughness K_c. Basing on the quantity of G_c it was possible to write the energy condition of a failure in case of a three-dimensional state of stress and then to determine the permissible areas of the stress intensity factors K_I , K_{II} , and K_{III} for typical welded joints. Exceeding these values results in cracking of welds.

For the above mentioned models of welds, the crack propagation rate has been also analysed using the equivalent stress intensity factor as a function of ΔK_I , ΔK_{II} , and ΔK_{III} . The life of typical welded joints with defects was determined. The obtained results may constitute new criteria in designing of welded structures.

Introduction

Present criteria of cracking of materials concern individual types of load. Permissible values of stress [1] and permissible values of defects are determined on the basis of critical values of K_{Ic}, K_{IIc} and K_{IIIc}. Life of particular structural components can be determined on the basis of crack growth rate which is in most cases determined only by means of one value, that is ΔK_I , ΔK_{II} or ΔK_{III} [2] or, in some cases, by means of two values, that is ΔK_I and ΔK_{II} [3,4].

The aim of the present work was to establish the energy condition of cracking in case of three-dimensional state of stress and to determine areas of permissible values of K_I , K_{II} and K_{III} for typical welded joints. Life of typical welded joints with defects has also been determined making use of an equivalent stress intensity factor expressed by ΔK_I, ΔK_{II} and ΔK_{III}.

Models of welds

Models of mild and low alloy, single and multi-layer welds representing features of their microstructure /fig. 1/ [5] have been proposed. In a single-layer weld model /fig. 1a/ we have assumed presence of dual microstructure with primary ferrite content /PF/ varying from 20 to 80 % and acicular ferrite content /AF/ varying from 80 to 20 %. Interdentritic distances d_D varied from 20 to 80 μm and volume fraction of model inclusions V_w changed from 0 to 1 %. The above mentioned values correspond to actual C-Mn SA-welds made by means of heat input varying from 2 to 8 MJ/m [6] .

In the model of multi-layer weld /fig. 1b/ we have assumed presence of grain-refined area with single-phase microstructure /GR/ and non-refined area comprising primary ferrite PF and acicular ferrite AF. Depending on the number of layers the volume fraction of non-refined microstructure varied from 30 to 50 % [7] .

For these models of welds, applying the Barenblatt's formula [8], the fracture toughness K_c has been calculated. It has been assumed that R is plastic strain zone and σ_{coh} is the yield point of particular microstructural components :

$$K_c = \sqrt{\frac{2}{\pi}} \int_0^R \frac{\sigma_{coh}/r/dr}{\sqrt{R-r}} \qquad /1/$$

The results of these calculations have been presented in table 1.

Energy condition of cracking in case of three-dimensional state of stress

The condition of propagation for a crack [8,9] which front is subjected to all three modes of deformation, that is opening mode I, shear mode II and tear mode III, is known as :

$$G_c = \frac{1}{E} / K_I^2 + K_{II}^2 / + \frac{1+\nu}{E} \cdot K_{III}^2 \qquad /2/$$

where :

$G_c = \frac{K_c^2}{E}$ - critical energy of decohesion

ν - Poisson's ratio

E - longitudinal modulus of elasticity.

Thus, if the formely determined value of K_c is known, and if the value of G_c is found, it is possible to write the energy condition of cracking in an elliptical form :

$$\frac{K_{II}^2}{G_cE - K_I^2} + \frac{K_{III}^2 \, / 1 + \nu /}{G_cE - K_I^2} = 1 \qquad /3/$$

which enables us to determine areas of permissible values of K_I , K_{II} and K_{III}. Values which have been determined for typical welded joints with defects are shown in table 1 and in fig. 2. The value of K_I has been found on the basis of [8] . Taking into consideration the fact that the ratio of defect dimension "a" to the plate width was equal to 0.0075, it can be assumed that the correction coefficient was constant and for the plate with a central defect it was $Y = 1$ and for the plate with one or two side defects it was $Y = 1.122$.

Life of welded joints in case of three-dimensional state of stress

If we write the formula /2/ in a form of increments of ΔK, we obtain as follows :

$$\frac{\Delta K_e^2}{E} = \frac{1}{E} \; / \; \Delta K_I^2 + \Delta K_{II}^2 \; / + \frac{1+\nu}{E} \; \Delta K_{III}^2 \qquad /4/$$

Thus it is possible to determine an equi-valent range of stress intensity factor ΔK_e :

$$\Delta K_e = \sqrt{/ \; \Delta K_I^2 + \Delta K_{II}^2 \; / + / \; 1 + \nu \; / \; \Delta K_{III}^2} \qquad /5/$$

Applying the Paris's formula [10] , defining the crack growth rate, it is possible to calculate the number of cycles to failure for a particular component of a welded structure :

$$N_f = \frac{1}{C} \int_{a_o}^{a_c} \frac{d\,a}{\Delta K_e^m} \qquad /6/$$

where : a_o - initial length of crack

a_c - critical value of crack equal to $\frac{K_c^2}{2\pi\sigma^2}$

Applying the formulae /5/ and /6/, and making use of definitions of K_I , K_{II} , K_{III} given in [8] , and also assuming, with a certain simplification, that $C = 5 \cdot 10^{-10}$ and $m = 2$ acc. to [5] , the value of N_f has been calculated from the formula :

$$N_f = \frac{10^{10} \cdot \ln \frac{a_c}{a_o}}{5 \cdot \pi \left[\Delta\sigma_y^2 \cdot Y_I^2 + \Delta\tau_{xy}^2 \, Y_{II}^2 + /1+\nu/ \Delta\tau_{yz}^2 \, Y_{III}^2 \right]} \qquad /7/$$

Taking into account that the ratio of defect dimension "a" to the plate width equals to 0.0075, it has been assumed that the correction coefficients are constant and for the plate with a central defect it was $Y_I = Y_{II} = Y_{III} = 1$, whereas for the plate with one or two side defects it was $Y_I = Y_{II} = 1.122$, $Y_{III} = 1$. Moreover, it has been assumed that $\Delta\sigma_y = 100$, 200 and 300 MPa, $\Delta\tau_{xy} = \Delta\tau_{yz} = 0.6\,\Delta\sigma_y$ and $\nu = 0.3$. The results have been presented in table 2. As it is stated in work [5] the microstructure of welds has insignificant effect on their crack growth rate. Therefore the assumption of the same values of C and m for various welds, in respect of their microstructure, is justified.

Discussion

It has been found that the area of permissible values of K_I, K_{II} and K_{III} is limited by a sector of the ellipsoid having the following coordinates :

$$\left(E\,G_c\right)^{1/2} \quad , \quad \left(E\,G_c\right)^{1/2} \quad , \quad \left(\frac{EG_c}{1+\nu}\right)^{1/2}$$

or, in case of the known value of K_I, it is limited by a sector of the ellipse which coordinates are :

$$\left(G_c E - K_I\right)^{1/2} \quad , \quad \left(\frac{G_c E - K_I^2}{1+\nu}\right)^{1/2}$$

That permissible area depends mainly on mechanical properties of the material of the weld and they are represented by the value of K_c. For example, an increase of primary ferrite content PF in single-layer welds varying from 20 to 60 % results in a decrease of permissible values of K_{III} by approx. 20 % and the value of K_{II} also by approx. 20 % at the value of $K_I = 46$ MPa $\sqrt{m}$ in case of a weld with a defect situated centrally, and such, which is subjected to loading according to modes I, II and III. An increase of the number of layers does not increase considerably the permissible area of K_I, K_{II} and K_{III}.

Life of a welded joint which is simultaneously subjected to cyclic loading with modes I, II and III also depends on mechanical properties of the weld and the value of an amplitude of the applied stress. These values are represented by constants C and m in the Paris's formula. If the value of the amplitude $\Delta\sigma_y = 100$ MPa and value of $\Delta\tau_{xy} = \Delta\tau_{yz} = 0.6\,\Delta\sigma_y$, the primary ferrite content PF changes from 20 to 60 % resulting in a decrease of the number of cycles to failure by approx. 30 %.

Conclusions

1. The presented above procedure enables us to determine the permissible area of values of K_I, K_{II} and K_{III}, as well as to evaluate the possibility of failure of welded structures when the load and defects are known.
2. Safety range can be determined by a comparison of an assumed life of the welded structure with the permissible number of cycles for given geometry conditions and load of the welded structure.
3. It has been ascertained that presence of primary ferrite in microstructure has unfavourable effect on the area of permissible values of K_I, K_{II} and K_{III}. An increase of primary ferrite content up to 60 % results in a significant limitation of area of permissible values and a decrease of life of welded structures by approx. 30 %.
4. It has been also found that the effect of the number of layers in welds on the area of permissible values of K_I, K_{II} and K_{III} and on their life N_f is insignificant.

References

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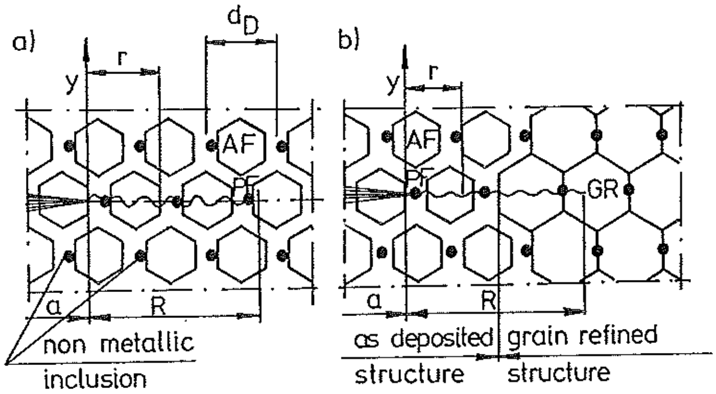

Fig. 1. Models for calculation of critical stress intensity factor K_c of : a/ single-layer welds, b/ multi-layer welds.

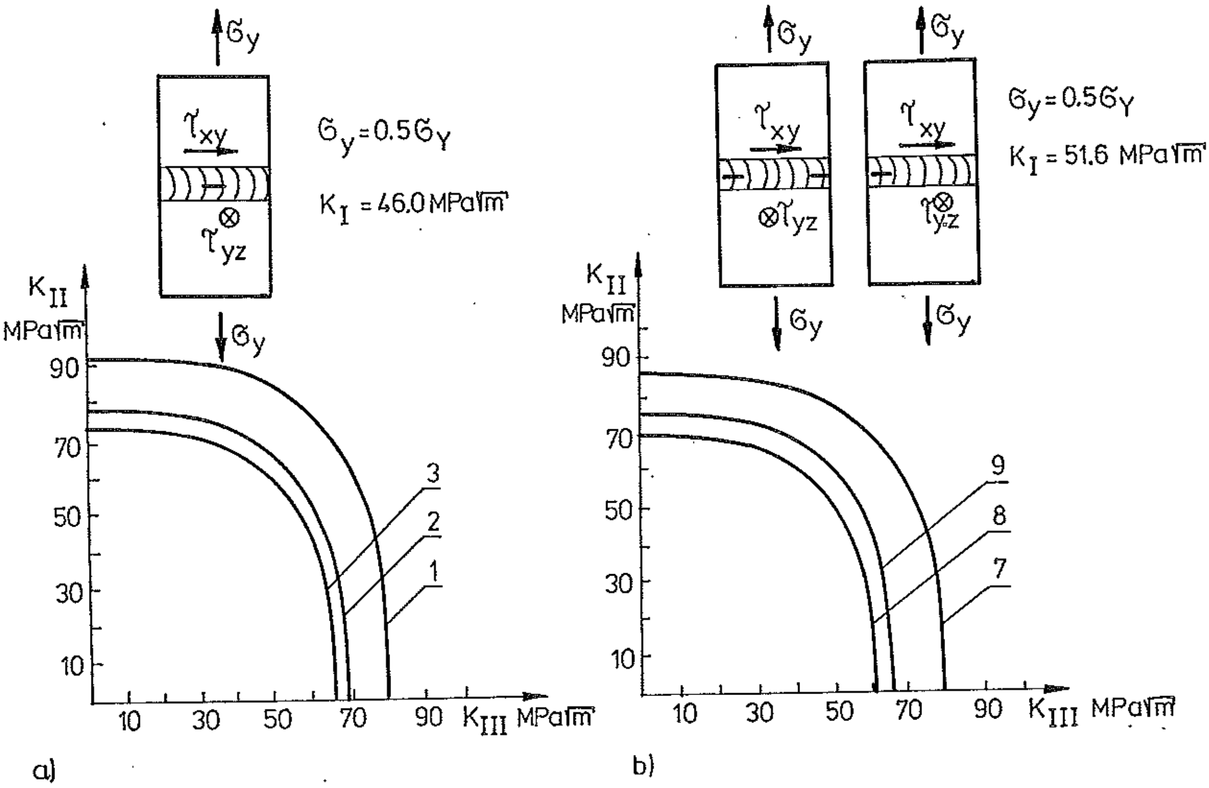

Fig. 2. Permissible values of K_I, K_{II} and K_{III} depending on kind of weld : a/welded joints with central defect, curves 1, 2,3 acc. to table 1; b/ welded joints with side defects, curves 7,8,9 acc. to table 1.

combination of these - all of which introduce biaxiality as shown in Figs 3 - 4.

A programme of research relevant to fatigue in steel offshore structures has been completed, and some of the details of this investigation are presented below.

OUTLINE OF THE TESTS

Nine large, full-scale specimens were fatigue tested in the as-welded or stress relieved states, as shown in Table 1. The plate material was originally developed for the fabrication of tubulars to be used in the construction of the Mackerel, Tuna and Snapper oil platforms located in the Bass Strait off the south-east coast of Australia. Specimens 2, 5, 7 and 9 were subjected to post-weld heat treatment, subsequent to the brace to chord welding, to relieve residual stresses. This was done in accordance with Australian Standard AS1958-1976, 'SAA Submarine Pipeline Code', which specifies a heating rate of 100°C/h, a holding time at 590°C - 620°C for one hour per 25 mm wall thickness, followed by cooling at 150°C/h to 300°C, finally ending by cooling in still air.

Comprehensive strain gauging was used, including single element, rosette, and crack propagation gauges, as illustrated in Fig. 5. Gauges were positioned on the outside and inside surfaces for both brace and chord. Two systems were used to monitor gauge response: a 200 channel Hewlett Packard automatic data acquisition, and a 200 channel Intercole Systems Compulog. A number of utility computer programs (FORTRAN) were written to process the data into more meaningful form.

A specially designed loading rig allowed the simultaneous operation of three hydraulic jacks. The combination of jacks provided a dynamic capacity of -50 to +50 tonnes operated by a servo-hydraulic Amsler P960 pulsator, an accumulator, and a dynamometer system. The specimens were bolted in place by flanges welded to the ends of the tubes.

Finite element methods (FEM) were also used to determine SCFs. While many element types are available to analyse shell structures, as in tubular joint intersections, Irons and Ahmad's isoparametric semi-loof shell and beam elements were found to be the most appropriate to determine the location and magnitude of hot-spot stresses (5, 6). The shell element is based on thin shell assumptions, in which each tubular member is represented by a cylindrical surface at the mid-thickness. This leads to some difficulties when results are compared with those obtained by experimental methods, as shown in Fig. 6.

RESULTS

A. Static Response

Testing commenced with incremental static loading, which allowed the determination of the load transfer characteristics and stress and strain concentration factors: SCF and SNCF respectively. The biaxial stress states are illustrated by Fig. 7 for the T type specimen, and by Figs 8 - 10 for the TK specimen.

Experimentally determined SCFs and SNCFs are presented in Figs 11 - 12 and in Table 2. The differences between SCFs and SNCFs are attributed to the existing biaxial stress states. Using only single element gauges to determine SCFs, the values were up to 20% higher than those of the SNCFs relying on the data obtained from the rosette gauges.

B. Finite Element Results

The biaxiality is greatly influenced by the geometries of the tubulars involved, and thus the SCFs are also affected. The FEM of analysis predicted the SCFs with reasonable accuracy, as shown in Fig. 13, while the influence of tubular geometries is illustrated by Figs 14 - 15.