TWO DIMENSIONAL FE-CALCULATIONS OF CRACKED X6CrNi1811-WELDMENTS STRETCHED MONOTONICALLY TO LIMIT LOAD

B. Rüttenauer

**INTERATOM, Internationale Atomreaktorbau GmbH,
Federal Republic of Germany**

ABSTRACT

For components of the primary coolant system of LMFBR's the demonstration of integrity against anticipated single peak loads is postulated. Within correlated programs experiments with cracked structures have been carried out yielding overall limit strains between 10 % and 15 %. The purpose of the analysis is the numerical simulation of structures strained to such levels. Load versus displacement-diagrams and load versus J-diagrams up to the limit load are calculated. By this way the influence of geometric parameters may be assessed in the post yield region. It is proposed to use such calculations to correlate experiments carried out with small specimens to experiments simulating the true dimensions of the design structure.

KEYWORDS

Elastic plastic fracture mechanics; finite element method; J-integral, approach to limit load.

INTRODUCTION

The application of the finite element method is meanwhile well established within fracture mechanics. In this paper the calculations are carried out upto the limit load, as would be desirable for predicting the behaviour of cracked structures against single overloads. The fact that stable crack growth is onset before reaching a critical value of Jc or the limit load, is taken into account by assuming a crack size that is properly enlarged against the initial crack that may have passed the quality control screening. Another limitation of the common first order formulation may lie in the gross strain reached in the X6CrNi1811 weldment, which goes well beyond 15 %. Therefore a special material law was used, which - partly - compensates for the failures of first order calculations. By comparing the results with forthcoming calculations which include all geometric nonlinearities, it is intended to define a limit of strain, up to which the first order formulation will suffice.

FINITE ELEMENT REPRESENTATION

The structures chosen for the first calculations were plates with the dimensions 40 x 80 mm and 8.8 x 17.6 mm, respectively, with central cracks of 1/10th of the width. The former are great enough to simulate portions of the original vessel. The FE-calculations are carried out with the computer code ADINA, that is the structure is decomposed in 8-node-isoparametric elements. The crack tip is modelled with triangular quarterpoint elements, as described by Barsoum (1977). Within the crack tip elements 3 x 3 integration points are used, and 2 x 2 points in the remaining mesh. The limit of the linear theory is defined by the start of plasticity in one of the crack tip element central integration points. The crack tip nodes are constrained to one common degree of freedom for the first - elastic - load step and thereafter allowed to move independently for the modelling of the appropriate singularity of the displacement field. The mesh has some 480 degrees of freedom, and the ratio of the size of the crack tip elements to the crack size is held constant to 1/20 independent of crack size.

Figure 1 shows the configuration of the elements surrounding the crack tip. The total width shown equals one fourth of the total crack length.

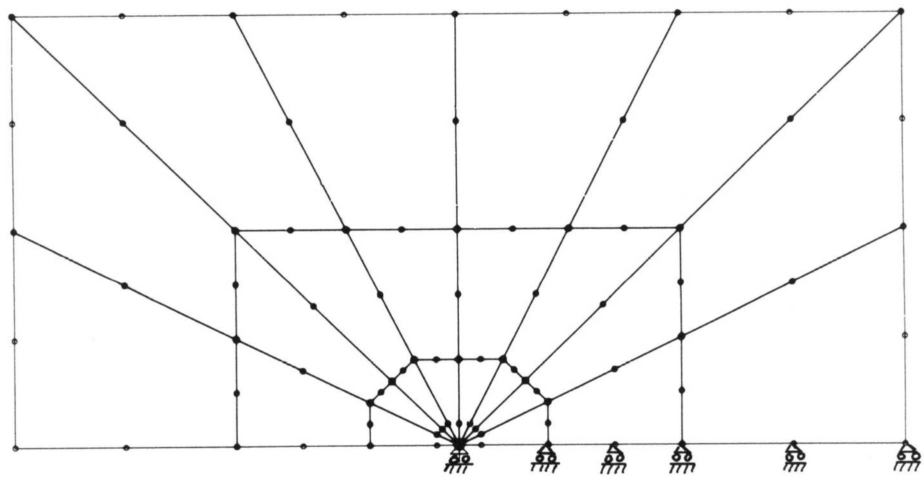

Fig. 1 Crack tip finite element configuration

METHOD OF EVALUATION

The elastic step of each calculation was verified by computing the stress intensity factor K using the stresses and displacements in the crack plane. This is compared with the theoretical K according for instance to Tada, Paris, and Irwin (1973) - and to the K value as computed by means of the J-Integral. See Fig. 2 for one example.

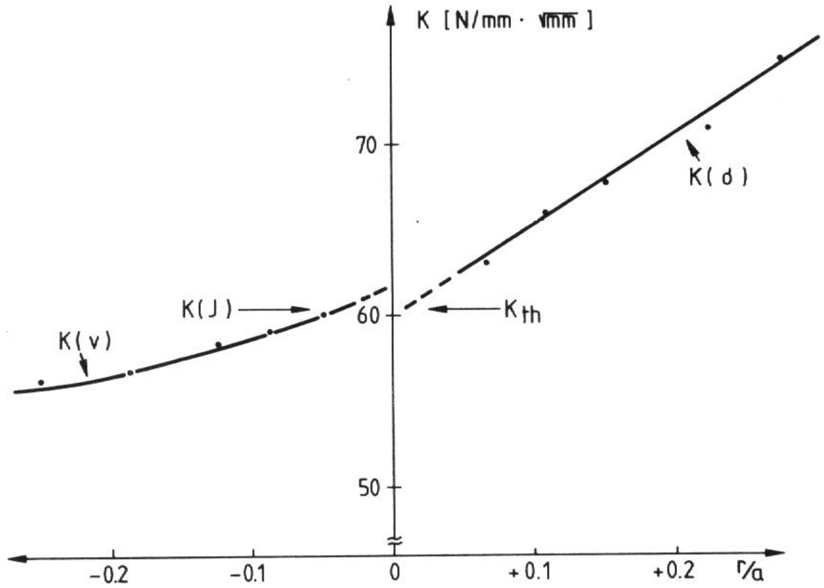

Fig. 2 K(v), K(J), K(σ) for a load corresponding to the limit of linear theory

The J-Integral is evaluated in the same manner as was shown to be appropriate for higher order elements by de Lorenzi (1978). Thus, for a first order F-E-solution:

$$J = \sum_{i=n}^{n} H(i) \Big\{ \Big(W - \sigma(1,1) \frac{\partial u(1)}{\partial x(1)} - \sigma(2,1) \frac{\partial u(2)}{\partial x(1)}\Big) \cdot My + \Big(\sigma(1,2) \frac{\partial u(1)}{\partial x(1)} + \sigma(2,2) \frac{\partial u(2)}{\partial x(1)}\Big) \cdot Mx \Big\}_i \qquad (1)$$

where i goes over the Gauß-points that define the integration path and the H(i) are the appropriate weights. W is the energy density, $\sigma(i,j)$, u(i), x(i) are the components of stress, displacement and location. In evaluating the differential transforms Mx, My full credit was taken from the fact that - besides the crack tip - only elements with straight sides and midpoint side nodes are used. Then, for instance, if the path is parallel to the local η-axis, Mx is simply given by:

$$Mx = \frac{1}{2}\left(\frac{x(1)+x(4)}{2} - \frac{x(2)+x(3)}{2}\right) + \frac{1}{4}\ \eta_o(x(1)-x(2)+x(3)-x(4)) \quad (2)$$

where the index denotes the four orner-nodes. The restriction does not affect the versatility, since it was shown by Nagtegaal, Parks and Rice (1974) that it is no trivial problem for the finite element-method to cope with the nearly incompressible plastic deformation. The 8-noded isoparametric element proved suitable only if restricted to straight sides.

A somewhat different scheme than that given by de Lorenzi (1978) was used for the integration around corners. The integral $\int_A^B ds$, as outlined in Fig. 3, is computed as the weighted mean of the adjacent path integrals:

$$\int_E^A ds = W(1) \int_A^B ds + W(2) \int_C^D ds + W(3) \int_D^E ds \quad (3)$$

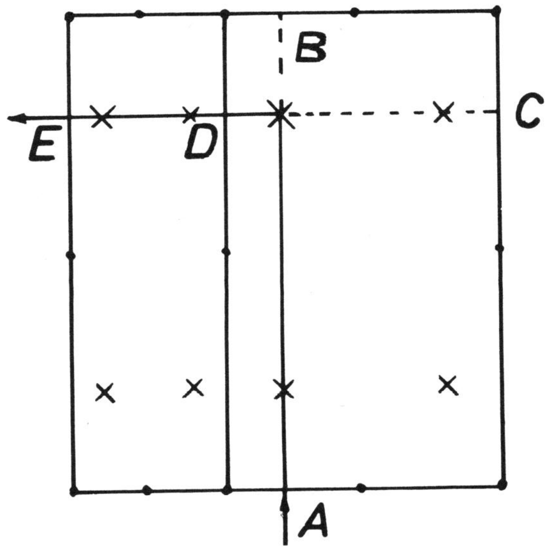

Fig. 3 Decomposition of the path integral around a corner

The weights depend on the number of integration points per element and the relative sizes of the adjacent elements. Around the crack tip, the elements are arranged so as to accomodate "natural" corners, see Fig. 1, so the approximation (3) is only needed farther away from the tip, where the integrand varies slowly.

FORMULATION OF THE MATERIAL LAW

Within the ADINA computer code the Lagrangian formalism is incorporated. Therefore it is possible to formulate an elastoplastic material model which would be the correct generalisation of the known Prandtl-Reuss equations with the von Mieses yield criterion to the case of finite strain. The main features of such a material model were outlined by Lee (1969) and Hibbit, Marcal and Rice (1970). Since the calculation of the then deformation dependent material moduli introduces a series of additional multiplications per each integration point and load step, as a first step the following procedure was used: The calculation is geometrically of first order, but the stress-strain curve, which is input for the Prandtl-Reuss equations, is evaluated in terms of technical stress versus technical strain. Through the definition of technical stress the deformation dependence of the material law is partly allowed for, if not correctly generalized to the multiaxial case.

DISCUSSION OF RESULTS

In Fig. 4 the load displacement curves for the plain strain and the plain stress case are shown, as obtained with a plate 40 mm wide, and a central crack with 2 mm half length. The stress strain curve was based upon measurements for X6CrNi1811 weldments at 450 °C. Figure 5 shows the load versus J-integral for the same case. Of course also the evaluation of the crack opening displacement is easily possible, yet it is not so useful, since a comparison with experiments is more difficult. To correlate experiments with different probe dimensions, the J-integral is used as the charakteristic parameter. That is, any value of load or displacement observed with a thin probe corresponds to that load or displacement of the thick structure, which belongs to the same value of the J-integral. The relation between the J-integrals belonging to the same load of different specimens is viewed upon as a geometry factor. Fig. 6 demonstrates that relation for two similar specimens. The width as well as the crack length of the minor specimen equal 0.22 times that of the other.

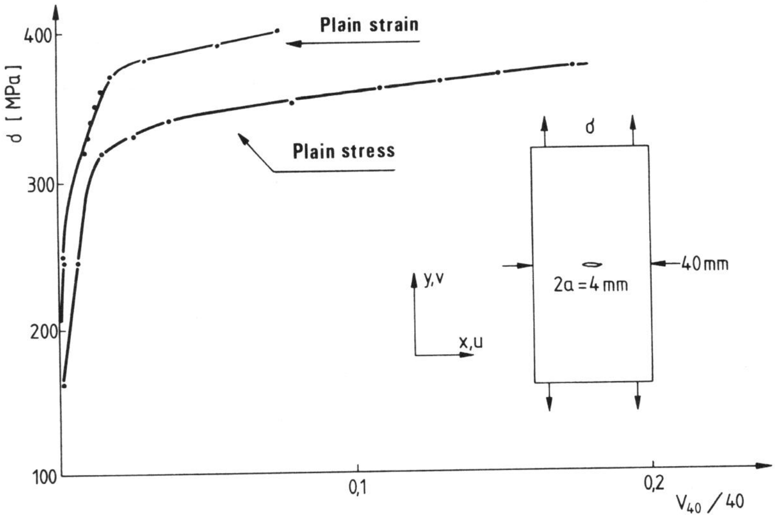

Fig. 4 Load versus displacement curves

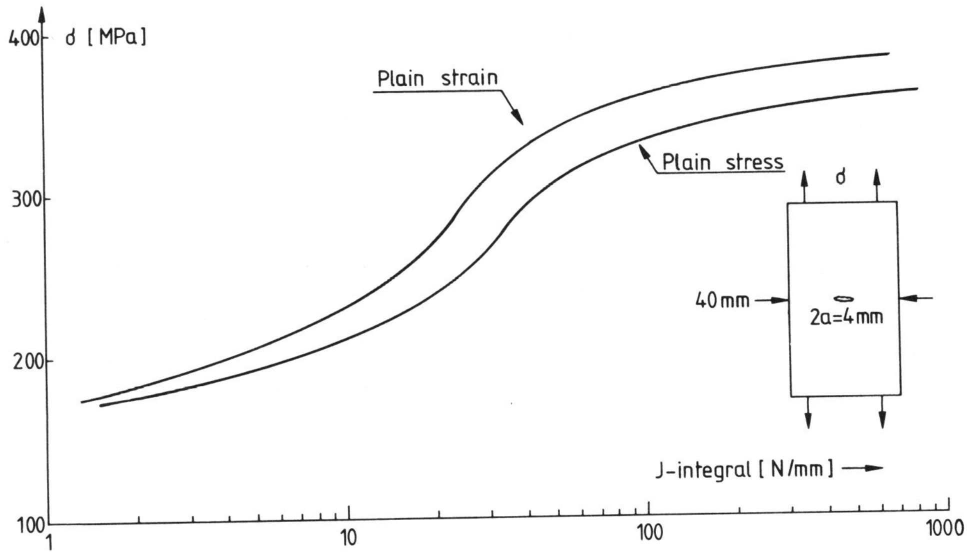

Fig. 5 Load versus J-curves

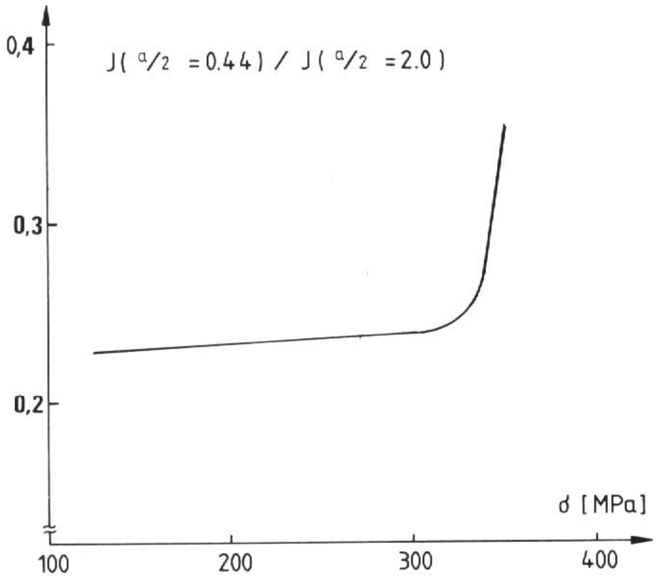

Fig. 6 Ratio of J-integrals for two different crack sizes, but the same crack size to thickness ratio

The question remains, whether the Material model described above is valid with the overall strains observed. If so, there should be no differences between Piola-Kirchhoff stresses and Cauchy stresses, and hence between geometrical linear and non-linear calculations. It was found that this is nearly so until the overall strains reach a value of 5 %, but the descrepancies between the resulting displacements increase steadily, if the strain goes beyond some 10 %. To assess a limit for the useful range of first order calculations, the same problems will be calculated with the Lagrangian formalism and a correct deformation dependent material law in the near future.

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