Numerical Evaluation of Creep Experiments

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ABSTRACT

The numerical evaluation of the creep fracture parameter C^* by the virtual crack extension method in analogy to the evaluation of the J integral is described. The methods are demonstrated by two- and three- dimensional finite element simulations including creep crack growth in plates and pipes with surface cracks. As for ductile fracture experiments, plane stress and plane strain simulations are bounds to the three- dimensional simulations which agree well with corresponding experiments.

INTRODUCTION

The transferability of fracture mechanics parameters obtained from laboratory specimens to structures under service conditions relies on a series of assumptions. The principal assumption is that the selected fracture parameter is only dependent on the material in its current status (e.g. temperature) but not on geometry. This geometry independence is usually demonstrated by testing different specimens under different loading conditions.

Several "one-parameter concepts" of fracture under high- temperature conditions have been proposed in recent years. Among them, the C^*-integral concept understood as an extension of the J- integral concept (Rice, 1968) of elastic plastic fracture mechanics seems to have the widest range of applicability, if large creep zones develop and stationary creep conditions can be assumed (Landes and Begley, 1976; Saxena, 1980; Nikbin *et al.*, 1984, Stonesifer and Atluri, 1982, Kienzler and Hollstein, 1986). For many materials the prediction of creep crack growth rates $\dot{a}$ by the concepts of stress intensity factor (Sadananda and Shahinian, 1978), reference stress (Nicholson and Formby, 1975), and path-independent integral J (Kienzler *et al.*, 1985) are applicable only with a large degree of uncertainty, whereas the C^* integral correlates with $\dot{a}$ over several orders of magnitude within a narrow scatterband.

Both integrals, J and C^*, have the same theoretical basis, since they follow from translational invariance of the general energy balance of continuum mechanics. If a nonlinear elastic material is described by a power law and a nonlinear viscous elastic material is described by a Norton power law, J and C^* can be calculated simultaneously by replacing energy and displacements with their respective rates (Kienzler *et al.*, 1985).

The original definition of C^* is given as a line integral (Landes and Begley, 1976) but C^* may be interpreted as the creep work dissipation rate if elastic strains are neglected:

$$C^* = \int_\Gamma \left(\dot{W}^* dy - T_i \frac{\partial \dot{u}_i}{\partial x} ds \right) = -\frac{1}{B}\frac{\partial \dot{U}^*}{\partial a} \qquad (1)$$

with the stress work rate $\dot{W}^*$ given by

$$\dot{W}^* = \int_0^{\dot{\varepsilon}_{mn}} \sigma_{ij}\, d\dot{\varepsilon}_{ij}\,, \qquad (2)$$

and $\dot{U}^*$ defined by

$$\dot{U}^* = \int_0^{\dot{V}} F\, d\dot{V}\,. \qquad (3)$$

Γ is an integration path around the crack tip connecting the lower and upper crack face counter-clockwise, T is the traction vector on Γ, B is the specimen thickness.

THE VIRTUAL CRACK EXTENSION METHOD

Especially for FEM applications the virtual crack extension method was introduced by Parks (1974, 1977) and later modified by DeLorenzi (1982). It was herewith possible to extend the J-integral concept to three-dimensional situations to obtain local values along a three-dimensional crack front.

The equivalence of the path- independent integral and energy-release rates is strictly valid only in very limited cases. So, Parks and DeLorenzi established their formulations within certain limitations, e.g. deformation plasticity, isothermal loading, etc. Several authors have proposed correction terms usually in the form of volume integrals to recover "path-independence" also for other loading cases. Recent re-evaluation of the theoretical background (Schmitt and Kienzler, 1988) revealed that independence from the integration regime may always be recovered if suitable area (2D) or volume (3D) integrals are evaluated over the interior of the contour or surface. This even holds if the material is described by an incremental law of plasticity. Since application of the virtual crack extension method involves integration over this area or

volume anyway, the additional numerical effort arising from these correction terms is usually negligible.

For isothermal static cases in the absence of body forces the IWM formulation (IWM-CRACK, 1988) is given by

$$J = -\frac{1}{\Delta A}\sum_{El}\sum_{Gp} w_{Gp}\left[\frac{\partial \det J}{\partial \Delta X} W + \det J\, \sigma^T \frac{\partial [B]}{\partial \Delta X} u\right] \qquad (4)$$

ΔA is the virtual change in crack area due to ΔX, ΔX is a virtual displacement pattern, detJ is the Jacobian determinant, [B] is the strain-displacement matrix. The summation is taken over all Gauss points (Gp) in all elements (El). The numerical evaluation of the creep integral C^* is similar to the evaluation of J. In this case the appropriate rates of W and u, $\dot{W}^*$ and $\dot{u}$, must be inserted in the above equations:

$$C^* = -\frac{1}{\Delta A}\sum_{El}\sum_{Gp} w_{Gp}\left[\frac{\partial \det J}{\partial \Delta X} \dot{W}^* + \det J\, \sigma^T \frac{\partial [B]}{\partial \Delta X} \dot{u}\right] \qquad (5)$$

SIMULATION OF CRACK GROWTH

In order to include crack propagation in the finite element simulation a method developed by DeLorenzi (1978) was modified and extented to three-dimensional applications. The crack propagation law of the model is either an experimental observation, e.g. a measured displacement vs crack growth curve $V(\Delta a)$, or a material resistance curve, e.g., $J(\Delta a)$ or $C^*(\dot{a})$. According to this law the crack tip nodes and the respective side nodes are shifted by small increments. In the following load step the stiffness matrix is reformed. The stresses and strains of the preceding load step are used as starting values for the equilibrium iterations. When the corner node of the next element is reached the two nodes of the first element are released. This procedure allows small amounts of crack-extension increments to be produced even with relatively coarse finite element grids, which is important for 3D applications.

RESULTS

<u>Two- and three- dimensional evaluation of C^*</u>

A series of side-grooved compact specimens of the material Inconel 617 were tested at T = 900°C (Kienzler and Hollstein, 1987). The experimental evaluation of C^* from

$$C^* = \eta \frac{F \dot{V}}{B(w-a)} \qquad (6)$$

utilized η-factors (Webster, 1983)

$$\eta = \frac{n}{n+1} \left(2 + 0.52 \frac{w-a}{a}\right) \tag{7}$$

which are essentially the same for plane strain and for plane stress (n is the exponent in the Norton creep law).

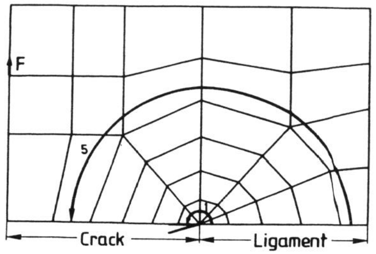

Figure 1: FE-mesh (2D) for a compact specimen

2D and 3D finite element simulations of the experiment have been performed in order to calculate C^* as a function of time. The 2D mesh is shown in figure 1, a three layered 3D mesh is developed from the plane mesh without modelling the side grooves. By utilization of the symmetries only one quarter of the specimen is modelled. Local values of C^* are obtained at four points along the half-thickness. In figure 2, C^* is plotted vs. thickness at different times. C^* takes its maximum value at the center and drops towards the free surface of the specimen. The time relaxation of the stresses causes a decrease

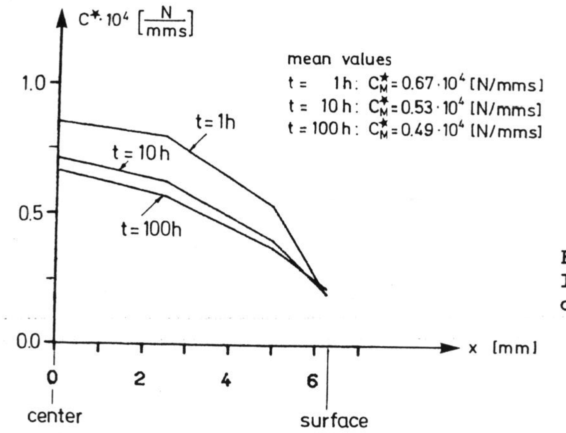

Figure 2: CT specimen, IN 617: C^* as function of thickness

of C^* with time. After about 30 h, C^* takes a constant steady state distribution across the thickness. For comparison with experiments and with 2D calculations the average of the calculated 3D variation across the thickness is taken and plotted in figure 3. The agreement between the 3D simulation and the experiment is rather astonishing. The measured creep crack extension came to less than 0.5 mm during the whole experiment, such that the utilization of a stationary model seems adequate.

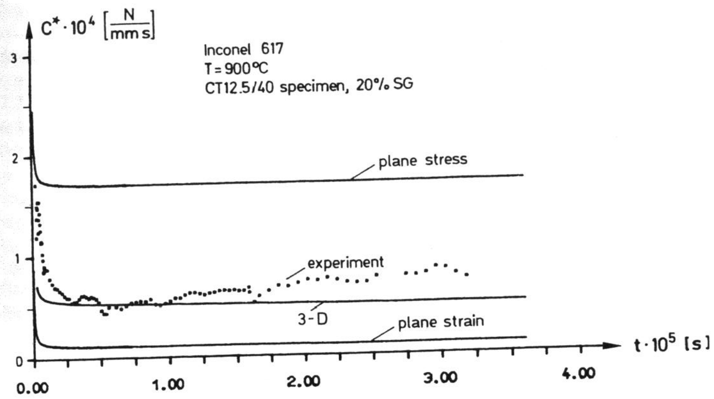

Figure 3: CT specimen, IN 617: Experimental and numerical evaluation of C^* vs time

In a test series with Incoloy 800 H at 800°C (Hollstein and Kienzler, 1988; Rödig *et al.*, 1987), tension specimens were tested with semi-elliptical surface cracks in order to demonstrate the transferability of results from standard specimens to more realistic structures. Unlike for the plane specimens η-factors are not available for 3D crack geometries. The adequate evaluation of these experiments relies therefore on the numerical analysis. Figure 4 shows a part of the finite element mesh utilized. Figure 5 shows the variation of C^* along the crack front for different times.

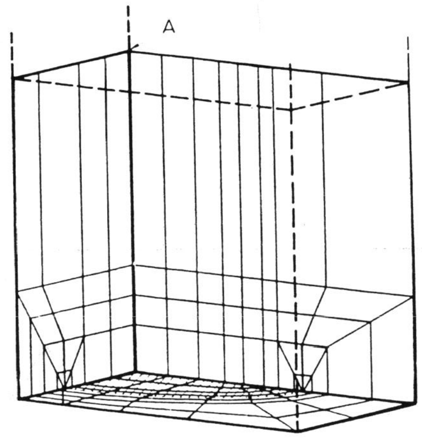

Figure 4: PTC specimen: Contour plot of FE mesh (3D)

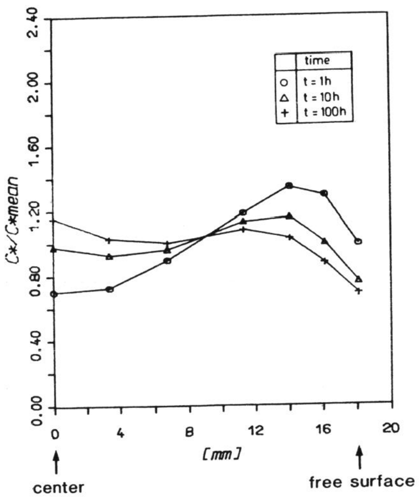

Figure 5: PTC specimen, Incoloy 800 H: C^* along the crack front

This variation changes significantly with time until steady state conditions are reached. By comparing the mean values of these distributions with a formal evaluation of C^* according to eqn. 6 η for this geometry could be calculated, η = 0.17, and used to evaluate further experiments. The results of the PTC experiment evaluated with this η- factor fit into the scatter band of the $C^*(\dot{a})$ correlation of a variety of creep crack growth experiments (figure 6).

Simulation of creep crack growth

Creep crack growth experiments with the steel 21 CrMoNiV 5 7 were carried out at 550°C with side-grooved compact specimens of different sizes (Hollstein and Kienzler, 1987). Finite

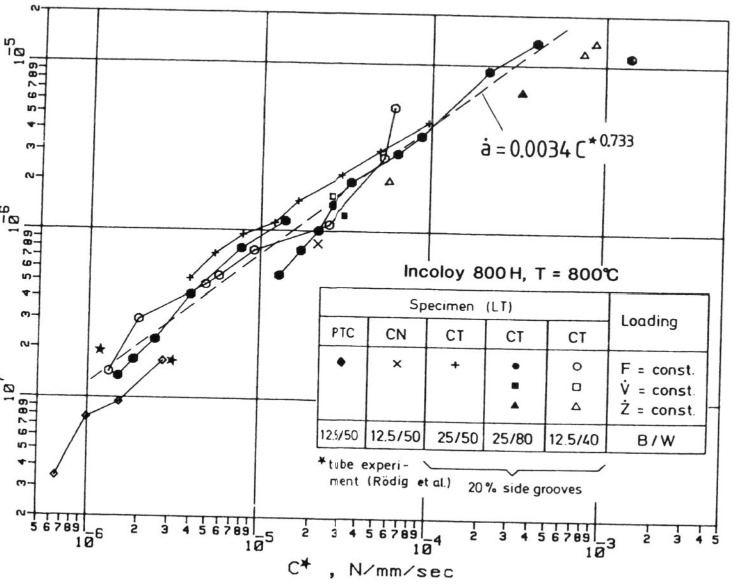

Figure 6: Incoloy 800 H: Crack growth rates $\dot{a}$ vs C^*

element simulations of these experiments with and without consideration of crack propagation were performed in order to validate the numerical procedures described in section 3. The evaluation of the creep integral C^* in the experiment was done according to eqn. 6 using $\dot{V}$, the measured load line displacement rate due to creep only. The material law utilized in the finite element simulations was of the Norton type, thus modelling only secondary creep. Therefore, the early times in the experiment are not well represented by this choice of material law. The crack growth law $C^*(\dot{a})$ was approximated from a series of experiments not taking into account significantly retarded crack propagation in the starting phase of the experiments.

Figure 7 compares measured and calculated C^* vs time curves from one typical experiment and from the finite element simu-

lations. The calculations without consideration of crack growth ($\dot{a}$ = 0) significantly underestimate C^* for all times. Even the plane strain solution (EDZ) with consideration of crack growth fails to meet the experimental curve by about one order of magnitude. The plane stress solution including crack propagation (ESZ, $\dot{a} \neq 0$) compares fairly well with the experimental curve. At early times, the above mentioned deficiencies in the creep law and in the crack- growth law explain the fact that the actual C^* is underestimated by about a factor of less than 2. Keeping in mind the usually large scatter in experimental creep crack growth curves this deviation seems acceptable. At later times the agreement between analysis and experiment is surprisingly good, the curves are nearly parallel with the ESZ-simulation above the

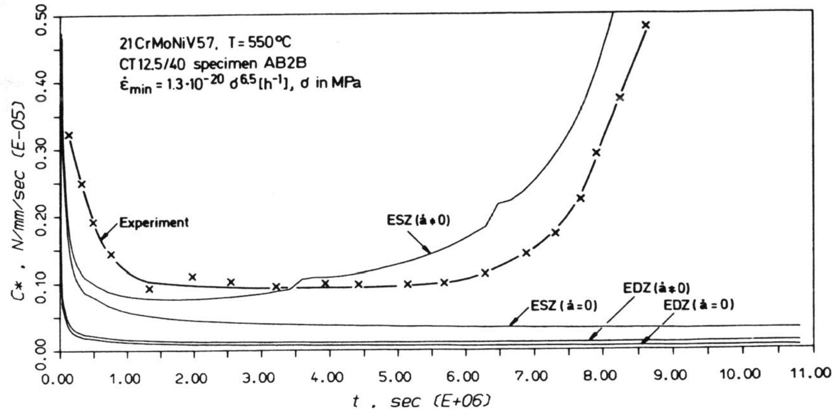

Figure 7: CT specimen, 1% Cr steel: experimental and numerical evaluation of C^* vs time

experiment. Considering the small thickness dimensions of the specimen of 12,5 mm the plane stress model must be considered most appropriate. Realizing that if crack growth is included in the models, plane strain and plane stress give lower and upper bounds to the experiments, an even better quantitative agreement may be expected from three- dimensional calculations.

CONCLUSIONS

Reliable finite element methods based on the virtual crack extension method and the node shifting and releasing technique are available to simulate creep fracture experiments numerically.

Stationary models (i.e. without taking into account crack growth) are applicable for the first part of an experiment where creep crack growth does not play an important role. If crack crowth is significant, however, it must be included in the models. Plane strain and plane stress yield lower and upper bounds to the experimental results, e.g. the $C^*(t)$- curve. As in elastic- plastic fracture mechanics three- dimensional models are required to ensure quantitative agreement.

Improvements may be expected from better material laws covering also primary creep and from more accurate correlations between crack velocity and creep integral.

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