Heavy-Section Steel Technology Program: Recent Developments in Crack Initiation and Arrest Research*

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Abstract

Technology for the analysis of crack initiation and arrest is central to the reactor pressure vessel fracture-margin-assessment process. Regulatory procedures for nuclear plants utilize this technology to assure the retention of adequate fracture-prevention margins throughout the plant operating license period. As nuclear plants age and regulatory procedures dictate that fracture-margin assessments be performed, interest in the fracture-mechanics technology incorporated into those procedures has heightened. This has led to proposals from a number of sources for development and refinement of the underlying crack-initiation and arrest-analysis technology. This paper presents an overview of ongoing Heavy-Section Steel Technology (HSST) Program research aimed at refining the fracture toughness data used in the analysis of fracture margins under pressurized-thermal-shock loading conditions.

Introduction

Pressurized-thermal-shock (PTS) has emerged as a loading condition of dominant concern in the structural-integrity evaluation of pressurized-water-reactor (PWR) pressure vessels. This loading condition can result from activation of the plant safety injection system (SIS) in response to detection of a loss-of-coolant accident (LOCA) condition. The resulting injection of cool SIS water into the reactor vessel downcomer annulus generates the thermal-shock portion of the loading condition. Thermal stresses resulting from this thermal shock can be high. They constitute the major element of a transient stress field which has been shown capable of initiating cracks from pre-existing flaws, should the vessel material fracture toughness be sufficiently low.

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Pressure loading, while contributing in a minor way to crack initiation, is the dominant influence on the behavior of the crack once it has penetrated deep within the reactor vessel wall. Activation of the SIS in response to a small break LOCA can produce pressure recovery in the reactor vessel sufficient to drive the crack through the vessel wall. The major contribution to reactor vessel failure rates predicted in a PTS analysis come from a small number of dominant transients, many of which are characterized by recovery of high vessel internal pressures.

It will be evident from the above discussion of the PTS scenario that an accurate definition of the vessel material crack initiation and arrest toughness is essential to the PTS analysis process. A major segment of the Heavy-Section Steel Technology (HSST) Program is devoted to refining the definition of vessel material crack initiation and arrest toughness for the unique combination of conditions existing during a PTS event. This paper provides a status report on this ongoing research. Specific areas of research reviewed are (a) the effect of crack-tip constraint on crack-initiation toughness as influenced by both crack depth and biaxial loading and (b) the influence of vessel dynamic response following crack initiation on the subsequent crack-arrest behavior.

Shallow Flaw Fracture Toughness

Research conducted at the University of Kansas has demonstrated enhanced fracture toughness for shallow flaws in both A36 and A517 structural steels.^{1,2} This result is attributed to the loss of crack-tip constraint associated with the proximity of the free surface of the material to the crack tip. Enhanced fracture toughness for shallow flaws has a special significance in the analysis of PTS events since prior studies³ have shown that the majority of crack initiations originate from shallow flaws. Major elements of the HSST shallow-flaw task are devoted to (a) the generation of a shallow-flaw data base for the A 533, Grade B Class 1 material used in the construction of many U.S. PWR vessels, and (b) the development of technology for application of this data base to the analysis of reactor vessel response to PTS transients.

The HSST shallow-flaw tests are generating fracture-toughness data for A 533 Grade B Class 1 material for a range of crack depths of interest to the PTS analysis. Specimens used in the development phase of the testing are shown in Fig. 1. They are nominally 102 mm (4 in.) deep with widths ranging from 51 mm (2 in.) to 152 mm (6 in.). Analysis, preparation and testing of these specimens has been described by Theiss et al. in Refs. 4 and 5.

Use of such a large test specimen is expensive in terms of consumption of valuable, fully characterized test material. Selection of the test specimen size reflects a concern that the test results must be capable of interpretation in terms of both the absolute crack depth "a" and the relative crack depth a/w . Finite-element analysis of shallow-flaw specimens recently reported by Kirk et al.⁶ reinforces the need for this approach. They found that neither a nor a/w , when considered alone, provided an adequate basis for transfer of data from test specimen to differing application geometries. Their results show that interaction of the plastic field generated by stress concentrations at the crack tip with that generated by overall response of the structural section to the applied loading controls the magnitude of stresses ahead of the crack tip and, thereby, the fracture toughness.

Fig. 1 102 mm (4 in.) deep test beams with B/W ratios of 0.5, 1.0, and 1.5 used in the development phase of the A 533 B shallow flaw fracture toughness testing program.

Results obtained to date from the HSST shallow-flaw fracture-toughness testing task are shown in Fig. 2. The trend of increasing fracture toughness for shallow flaws in the lower-transition region is clearly evident. Lower-bound fracture toughness for cracks in the range 10 to 15 mm (0.4 in. to 0.6 in.) deep is seen to be more than double that for the deep cracks. The difference is seen to disappear at lower-shelf temperatures. This is consistent with trends observed in Refs. 1 and 2 for low- and high-yield strength material respectively.

Professor Rolfe of the University of Kansas has recently completed a comprehensive review⁷ of the HSST shallow-flaw task. The review identified actions required to validate the shallow-flaw fracture-toughness data for use in reactor vessel structural margin assessments. A key recommendation was for the analysis of both test specimens and cracked reactor vessels to confirm that the recently developed dual-parameter fracture-toughness correlations provide a basis for transfer of the test data. This analysis is currently in progress at the University of Maryland under the direction of Prof. C. W. Schwartz. Preliminary results from the analysis gave Q factors in a dual-parameter, J-Q fracture correlation of -0.135 and -0.95 for test specimens with a/w ratios of 0.5 and 0.1, respectively.

Fig. 2 Preliminary results from the shallow flaw testing program confirm the existence of enhanced fracture toughness for shallow flaws in A 533 B Class 1 material at temperatures in the lower transition range.

Constraint Effects in Ductile Tearing

PTS transient loading produces biaxial stress fields in the reactor vessel wall (Figs. 3 and 4). In the case of transient thermal stresses, which dominate the crack-initiation process for shallow flaws, the resulting ratio of principal stresses at the inside surface of the vessel is 1:1. These stresses are in a plane oriented parallel to the inner surface of the vessel. One of the principal stresses in this field is, therefore, aligned parallel to the crack tip for both axial and circumferential flaws. This was identified in Ref. 8 as a condition with the potential to degrade the material ductile-tearing toughness.

A number of researchers have addressed the issue of crack-tip constraint on fracture toughness, including the effect of biaxial stress fields. Their focus has been on the effects of in-plane biaxial stresses. Their research has produced the dual-parameter elastic K-T⁹ and elastic-plastic J-Q¹⁰ fracture-toughness correlations, which predict a significant influence of in-plane biaxial stresses on fracture toughness. The K-T and J-Q correlations, however, incorporate an assumption of plane-strain boundary conditions. Out-of-plane (OP) stresses considered in these dual-parameter fracture correlations are limited to those generated by the restraint of Poisson's ratio contractions. In their current state of development, therefore, the K-T and J-Q dual-parameter fracture correlations can not address the effect on ductile tearing toughness of large far-field OP stresses which are characteristic of the PTS loading condition.

Fig. 3 The three contributing stress distributions which together define the overall stress distribution in a pressure vessel wall during a PTS transient are all biaxial.

The HSST Program is investigating potential effects of prototypical PTS OP far-field stresses on fracture toughness using a variety of analytical models. Results obtained to date have been paradoxical in that the magnitude of the predicted effects of OP far-field stresses (and strains) appears to be dictated by the approach adopted in formulating the fracture models.

Models based on a detailed definition of the stress and strain fields in the region immediately adjacent to the crack tip predict a minimal impact of OP stresses and strains on fracture toughness. This is a reflection of the fact that the far-field strains represent a very small addition to the large strains already existing at the crack tip.

Fig. 4 One of the far-field principal stresses existing in a pressure vessel wall during a PTS transient is aligned parallel to the crack front of any axial or circumferential crack.

In contrast, a model that focuses on the effect of far-field OP stresses on the stress state at the boundary of the plastic zone, indicates there may be a significant effect on fracture toughness. This is due to the increase in the hydrostatic to Von Mises' equivalent stress ratio (σ_H/σ_e) generated by the additional OP stress increment and the pronounced effect of that increase on fracture ductility.

The following brief review of the analysis results will illustrate the nature of the paradox. A detailed description of the near-tip models used to investigate the effect of OP stresses and strains on fracture toughness has been given by Shum et al.¹¹ Both stress and strain-based fracture criteria were investigated.

A micromechanical model based on a generalized plane-strain adaptation of the Rice-Johnson analysis¹² of a blunted notch was used to evaluate the effect of far-field OP stresses on crack-tip stress triaxiality and fracture ductility. The model was used to predict strains and σ_H/σ_e in the near crack-tip region in the presence of an OP strain. Failure was assumed to occur when the strains reached a stress-state-dependent limiting value. An estimate for the fracture ductility under conditions of multiaxial stress was derived from data generated by MacKenzie et al.¹³ for a range of materials.

Results produced using this model are illustrated in Fig. 5. The model predicted that for the case of a zero-strain hardening material the applied strain would reach critical values in a

Fig. 5 A crack-tip micromechanical model predicts intersection of the material critical strain curve with the applied strain curves for a range of generalized plane strain boundary conditions in a region of the crack-tip plastic zone where the σ_H/σ_e value is less than 1.6. (From Ref. 11)

region of the plastic zone where σ_H/σ_e was in the range 1.5 to 1.6. Out-of-plane far-field strains in the range $\pm 2\%$ were predicted to have a $\pm 14\%$ influence on the critical strains. This variation in the critical strains was predicted to influence the fracture toughness by only $\pm 2\%$.

A second model, based upon a hollow-cylinder analogy, confirmed the stress-strain field results predicted by the micromechanical model. Any questions relating to results from these models would, therefore, appear to be confined to the fracture ductility estimates and/or the location within the plastic zone of the critical combination of applied strain and fracture ductility.

A third model was based on a dual-parameter cleavage fracture-toughness correlation previously demonstrated by Keeney-Walker.¹⁴ This model uses the area (A_{CR}) within a critical stress contour centered at the crack tip as a second correlation parameter. The effectiveness of this model in the plane-stress to plane-strain domain had been previously demonstrated in an analysis of the Oak Ridge National Laboratory (ORNL) Wide-Plate fracture test results, where unanticipated elevations in the K_I required to initiate fracture had been encountered.

Results produced by the J- A_{CR} model are shown in Fig. 6. An out-of-plane strain of +0.114 % is predicted to result in a reduction of 22% in J_{IC} , which translates to a reduction of less than 12% in K_{IC} from the reference plane strain value.

Fig. 6 The dual parameter J-ACR fracture toughness correlation predicts a 22% reduction in J_{IC} (12% in K_{IC}) relative to the plane-strain fracture toughness when a positive far-field strain of .114% is applied parallel to the crack front. (From Ref. 11)

A common result produced using diverse models gives reason for confidence in that result. Prior to acceptance of the result, however, it is prudent to verify that the models do not share any common features which could detract from their value as independent cross checks. The models discussed above are truly independent in the failure-criteria area in that one uses a critical stress and the other uses a critical strain to predict fracture. They do, however, share a common element in that they are all based on a detailed definition of the near-tip stress-strain field where strains are very high. A fourth model has been generated to investigate the possible significance of this common element of the prior models. The fourth model utilizes elastic analysis of the stresses in the region immediately adjacent to the crack-tip plastic zone to infer the influence of far-field OP stresses on fracture toughness. The model does not, therefore, share the common element described previously for the other models.

Elastic stresses on the plane of propagation of the crack are defined using the equations of Inglis,¹⁵ as interpreted by J. G. Merkle.* For the case of a center-cracked panel with zero crack-tip radius the equations are as follows.

*J. G. Merkle, "Special Technical Assistance, Chapter 4," in "Heavy-Section Steel Technology Program Semiannual Progress Report for April-September 1991," USNRC Report NUREG/CR-4219, Vol 8, No. 2 (ORNL/TM-9593/V8&N2) (to be published early in 1992).

$$\begin{aligned}
\sigma_y &= \sigma_y' \frac{\left(\frac{r+a}{a}\right)}{\sqrt{\left(\frac{r+a}{a}\right)^2 - 1}} \\
\sigma_x &= \sigma_y - \sigma_y' \\
\sigma_z &= \nu(\sigma_x + \sigma_y)
\end{aligned} \tag{1}$$

where: directions x, y, and z are as defined in Fig. 4.

a = the half length of a through-thickness crack in a plate
 r = is the distance along the x axis ahead of the crack tip
 ν = Poisson's ratio
 σ_y' = the far field opening mode stress
 σ_x , σ_y and σ_z are the three principal stresses along the x axis

Adaptation of the above equations for the present study can be made simply by adding the far field stress σ_z' directly to the constraint induced σ_z stresses. This is possible because the closed crack does not cause any perturbation of the far-field σ_z' stresses. The body is free to contract in both the x and y directions in response to a uniform σ_z' stress, and the σ_z' stress does not therefore generate any contribution to either the σ_x or σ_y stresses.

Equation 1 are for a panel of infinite dimensions with a center through crack of finite length $2a$. For the purpose of a study relating to PTS crack initiation, a more appropriate geometry is that of a single-edged notch in a finite-thickness plate. Equations 1 were converted to this form by incorporating the appropriate factors. The final form of the equations as used in the model was then as follows.

$$\begin{aligned}
\sigma_y &= 1.12 \times f(a/W) \times \sigma_y' \frac{\left(\frac{r+a}{a}\right)}{\sqrt{\left(\frac{r+a}{a}\right)^2 - 1}} \\
\sigma_x &= \sigma_y - \sigma_y' \\
\sigma_z &= \nu(\sigma_x + \sigma_y) + \sigma_z'
\end{aligned} \tag{2}$$

where

σ_z' is the far-field stress parallel to the crack front and
 $f(a/w)$ is a coefficient accounting for the effect of the finite-plate geometry on the stress-intensity factor.¹⁶

Equation (2) were used to calculate the Von Mises' equivalent stress (σ_e) and the hydrostatic stress (σ_H) for a potential part-through surface cracked biaxial test specimen for plane-stress, plane-strain and biaxial-loading constraint conditions. The far-field stress was set at 345 MPa (50 ksi) to match typical stress levels predicted at a depth of 6 mm (0.25 in.) beneath the inner surface of a vessel at the time of crack initiation during a PTS transient (Figs 3 and 4). A plate thickness of 76.2 mm (3 in.) was selected based on the loading capacity of potential test machines. The crack depth was selected as 23 mm (0.9 in.) to produce a crack tip K_I value of $154 \text{ MPa}\sqrt{\text{m}}$ ($140 \text{ ksi}\sqrt{\text{in.}}$). This value was selected to match the plane-strain fracture toughness reported by Landes and McCabe¹⁷ in a series of plane-stress and plane-strain fracture toughness tests on A 533 Grade B Class 2 material. Plane stress and plane strain fracture-toughness data produced under otherwise identical conditions were required as input to this scoping analysis model.

Fig. 7 shows the test specimens used in the study of Ref. 17. Out-of-plane stresses in the analysis of the plane-stress case were set at 20% of the plane-strain value rather than zero in recognition of the finite transverse dimension of the plane-stress specimen shown in Fig. 7.

Results generated using the modified Inglis equations are shown in Fig. 8. The elastically estimated crack-tip plastic-zone boundary is defined by the intersection of the equivalent-stress curve with the yield-stress line for each of the three boundary-condition cases considered. Corresponding σ_H/σ_e values at the elastic-plastic boundary are obtained as 0.68, 1.82, and 3.38 for the plane-stress, plane-strain, and biaxial-loading cases, respectively. The σ_H/σ_e value for the plane-strain case is similar to that obtained from the micromechanical model, as illustrated in Fig. 5. The biaxial-loading case, in contrast, gives a σ_H/σ_e value approximately double that given in Fig. 5.

The σ_H/σ_e ratio is of interest because of its influence on fracture ductility. Weiss¹⁸ has investigated the effect of stress state on fracture strain and proposed the following relationships:

$$\frac{\bar{\epsilon}_{F,\alpha,\beta}}{\bar{\epsilon}_{TF}} = (\omega m)^{1/n} \quad (3)$$

where

$\bar{\epsilon}_{F,\alpha,\beta}$ is the effective fracture strain (plastic strain) predicted in a triaxial stress field characterized by stress ratios α and β .

$\bar{\epsilon}_{TF}$ is the effective fracture strain in uniaxial tension

Fig. 7 Fracture toughness specimens from which the plane-stress and plane-strain fracture toughness data used in this study were generated. (From Ref. 17)

$$\omega = \frac{1}{1 + \alpha + \beta} \quad (4)$$

$$m = [(1 + \alpha + \beta)^2 - 3(\alpha + \beta + \alpha\beta)]^{1/2} \quad (5)$$

$$\alpha = \frac{\sigma_x}{\sigma_y} \quad \text{and} \quad \beta = \frac{\sigma_z}{\sigma_y} \quad (6)$$

Fig. 8 σ_H/σ_e stress ratios predicted at the crack-tip plastic zone boundary by the modified Inglis equations for plane-stress, plane-strain and 1:1 biaxial-loading boundary conditions.

n is the material strain hardening exponent in an equation for the effective stress-effective strain curve of the form:

$$\bar{\sigma} = k\bar{\epsilon}^n \quad (7)$$

Equations (3) through (6) require the input of principle-stress data from an elastic-plastic analysis for the crack tip plastic zone in order to provide estimates of the material fracture strain in the fracture zone within the plastic zone. Clearly, this input is not available from the elastic analysis results obtained using Eq. 2. Those results can, however, be used to predict the material fracture strain for the σ_H/σ_e value existing on the elastic-plastic boundary. Plastic strains at this location are, by definition, zero. The fracture strain at the boundary is of interest, therefore, only because it fixes one end of the fracture-strain curve within the plastic zone. This information is useful, however, in that it serves to indicate if potential tearing initiation sites may exist in regions of the plastic zone beyond the boundaries of the previously discussed crack-tip models.

Principal-stress data at the plastic-zone radius were input to Eqs. (3) through (6) to calculate the failure strain for each of the three constraint conditions considered. A value of 0.31

was selected for the strain-hardening coefficient (n). Results from this analysis are shown in Table 1. Note that the predicted fracture strain for the case of biaxial loading is approximately 14% (0.06/0.43) of that for the plane-strain case.

Table 1 Constraint-Dependant Fracture Ductility Estimates n = 0.31

Constrain condition	σ_x/σ_{ys}	σ_y/σ_{ys}	σ_z/σ_{ys}	α	β	$\frac{\bar{\epsilon}_{F,\alpha,\beta}}{\bar{\epsilon}_{TF}}$ %
Plane stress	0.64	1.27	0.11	0.51	0.09	10.41
Plane strain	1.79	2.41	1.26	0.74	0.52	0.43
Biaxial	3.35	3.98	2.82	0.84	0.71	0.06

Equation (2) can not provide any information on either effective strains or σ_H/σ_e ratios within the plastic zone. Large displacement elastic-plastic, finite-element analyses by Bass et al.* have, however, shown that σ_H/σ_e ratios remain at or above the plastic-zone boundary levels over a large portion of the plastic-zone radius. Relative fracture strains in the process zone for varying constraint conditions can, therefore, be inferred from the ratio of fracture strains established at the plastic-zone boundary.

Weiss¹⁸ has derived the following expression for fracture toughness in terms of the fracture strain under conditions of triaxial loading.

$$K_{Ic}^2 = \pi \sigma_y^2 \rho^* \left[\left(\frac{\bar{\epsilon}_{F,\alpha\beta}}{\bar{\epsilon}_y} \right)^{n+1} - 1 \right] \quad (8)$$

where σ_y is the opening mode stress at the plastic-zone boundary,

ρ^* is Neubers micro-support effect constant,

$\bar{\epsilon}_y$ is the effective plastic strain at the plastic-zone radius.

For the purpose of estimating the effect of departures from plane-strain constraint on fracture toughness, Eq. (8) can be simplified by eliminating all terms that remain constant for each of the constraint conditions. The resulting equation for the relative fracture toughness at a given constraint condition is as follows.

*B. R. Bass et al, "Constraint Effects on Fracture Toughness for Circumferentially Oriented Cracks in Reactor Pressure Vessels," USNRC Report NUREG/CR-5792 (ORNL/TM-11968) (to be published February 1992).

$$\frac{(K_c)_{NPS}}{(K_{Ic})_{PS}} = \frac{\left[\sigma_y \sqrt{\left(\frac{\bar{\epsilon}_{F,\alpha\beta}}{\bar{\epsilon}_y} \right)^{n+1} - 1} \right]_{NPS}}{\left[\sigma_y \sqrt{\left(\frac{\bar{\epsilon}_{F,\alpha\beta}}{\bar{\epsilon}_y} \right)^{n+1} - 1} \right]_{PS}} \quad (9)$$

where suffixes PS and NPS denote plane-strain and nonplane-strain constraint conditions, respectively.

Fracture-toughness results for A 533 B Class 2 material at a test temperature of 0°C (32°F) were obtained from the tests of Ref. 17 as 150 MPa·√m (137 ksi√in.) and 629 MPa·√m (572 ksi√in.) for the plane-strain and plane-stress "4 T profile" test specimens, respectively. Inserting these values, together with the corresponding parameters from Table 1 and a value of 0.001% for $\bar{\epsilon}_y$ into Eq. 9, gave the previously quoted value of 0.31 for n . This value of n was then used in Eq. 9 together with the plane-strain and biaxial-loading parameters from Table 1 to produce an estimate of 0.47 for the ratio of fracture toughness under biaxial loading and plane-strain constraint conditions.

The estimate of the effect of out-of-plane biaxial stresses given by the above scoping analysis differs significantly from that given by the crack-tip fracture models. At this time it would be inappropriate to attach any significance to the scoping-analysis results beyond recognition of the fact that uncertainty exists relative to the effect of out-of-plane biaxial loading. Biaxial test data will be required to calibrate and validate models for prediction of the effects of out-of-plane biaxial loading on fracture toughness.

Biaxial fracture toughness tests have been performed by S. J. Garwood¹⁹ at The Welding Institute in the United Kingdom. The specimen size used in those tests was such, however, that a condition of general yielding was generated in the specimen ligament. It will be evident from the above analysis that prototypical elastic conditions in the test-specimen ligament are necessary for the test data to be used to resolve the paradox outlined above.

Geometric irregularity of the crack front is a further factor which can temper the buildup of out-of-plane σ_z stresses and thereby influence the location within the plastic zone of the maximum σ_H/σ_E stress ratio. Crack front geometry data obtained from a post-test microscopic examination of one of the deep flaw ($a/w = 0.5$) specimens used in the shallow flaw task are shown in Fig. 9. The data cover a 12.7 mm (0.5 in.) length of the fatigue crack front at the center of the 102 mm wide specimen. Within that length the fatigue crack front exhibits considerable irregularity, with several projections in the range .08 to .11 mm (.003 to .0045 in.). These projections break the transverse load path at the crack tip and thereby inhibit the buildup of transverse stresses. The crack tip in effect is seen to be a zone rather than a point with a finite width in the direction of crack propagation. The diffuse nature of the crack tip will have the effect of reducing the rate of buildup of transverse stresses in the plane of crack propagation and displacing the maximum constraint condition further into the plastic zone.

Fig. 9 Data from the microscopic examination of the fracture surface from one of the deep cracked beam specimens show that the fatigue crack front is a zone rather than a point with a finite width in the direction of crack propagation.

The potential significance of geometric irregularity of the crack front can be gauged by comparing the crack front indentation data of Fig. 9 with the elastically calculated plastic zone radii from Fig. 8. The crack front indentations to plastic zone radius ratio is 4.5% (0.11/2.4) for the plane strain case but increases to 14% (0.11/0.8) for the biaxial loading case. This suggests that the location of the process zone within the plastic zone becomes increasingly sensitive to crack front geometric irregularity as the out-of-plane far field stress σ'_z increases.

The principal result to date from the HSST Program investigation of out-of-plane biaxial stress effects on fracture toughness has been the decision to perform a pilot series of biaxial, fracture-toughness tests. The specimen size will be such as to satisfy the ligament elastic condition requirement outlined above. This requirement will necessitate the use of a large biaxial test specimen and a test machine with matching capabilities.

Before leaving this topic, it is appropriate to place the concern for the potential effect of biaxial loading on fracture toughness into the correct perspective. It appears there is some potential for out-of-plane biaxial loading to reduce fracture toughness below what it would be under otherwise identical uniaxial loading. This should not be taken to infer that the resulting fracture toughness will be less than that obtained from compact test specimens meeting the size requirements of Ref. 20 for plane strain constraint. The compact-test specimen geometry provides a degree of crack-tip constraint higher than that associated with most cracked structures. It is unlikely, therefore, that the biaxial stress effects discussed above will reduce the fracture toughness under prototypical reactor vessel loading conditions below that obtained from

compact-test specimens conforming with the requirements of Ref. 20. An improved understanding of the effects of out-of-plane loading on fracture toughness is of value, however, in refining the probabilistic analysis of PTS transients where best estimate fracture-toughness properties for the actual crack tip constraint conditions can be used.

Dynamic Crack Arrest

Analysis of crack-arrest behavior in the present generation of PTS-analysis computer programs^{21,22} is based a vessel-wall stress distribution derived using static equilibrium loading of the cracked structure. Implicit in this approach is the assumption that dynamic conditions existing during the crack-propagation process will have no significant effect on the final configuration of the arrested crack.

It has long been recognized that intermediate arrest and reinitiation of a crack propagating in a large-scale structure will occur prior to its final arrest.²³ This arrest and reinitiation behavior occurs because the crack propagation time is so much less than the dynamic response time for large-scale structures.

Recent papers have presented crack-arrest analysis results obtained using a model reflecting some aspects of the dynamic crack-propagation process.²⁴⁻²⁶ This prompted a renewed HSST Program study of the propagation of cracks in a reactor vessel during a PTS event. This ongoing study is being conducted by Keeney-Walker and Bass* using the 180° thermoelastic-dynamic ADINA/VPF finite-element model of the reactor vessel wall shown in Fig. 10. The model incorporates an application-mode crack propagation capability which permits the crack to advance when the crack-tip stress-intensity factor exceeds the material dynamic fracture toughness K_{Id} . Estimates of K_{Id} as a function of crack-tip velocity and the local material temperature were taken from Refs. 27 and 28.

The model of Fig. 10 was used to analyze the dynamic response of a vessel with a 2.39 cm (0.94 in.) deep surface crack to a PTS transient. The transient and initial crack depth selected for this analysis were taken from a previously completed OCA-P analysis. The OCA-P analysis predicted crack initiation with a subsequent arrest when the crack had propagated to the mid point of the vessel wall.

Transient stress intensity-factor results from the initial dynamic analysis of the cracked vessel are shown as the lower curve in Fig. 11. The crack initiated when the crack-tip stress-intensity factor reached a value of $110 \text{ MPa}\sqrt{\text{m}}$ ($100 \text{ ksi}\sqrt{\text{in.}}$) and arrested after it had penetrated to a depth of 8.51 cm (3.35 in.). The vessel wall T-RT_{NDT} temperature and crack tip stress intensity factor at the initial crack arrest were 87.9°C (158°F) and $172 \text{ MPa}\sqrt{\text{m}}$ ($157 \text{ ksi}\sqrt{\text{in.}}$), respectively. The crack-tip stress-intensity factor continues to increase, following arrest of the crack, due to the dynamic overshoot effect. In addition, higher order vibration response of the reactor vessel generates an oscillation of the crack-tip stress-intensity factor time history about the mean value. These oscillations generate

*J. Keeney-Walker and B. R. Bass, "Issues in Dynamic Fracture Analysis of Reactor Pressure Vessels," USNRC NUREG/CR-5793 (ORNL/TM-11969), (to be published January, 1992).

Fig.10 The ADINA elastic-dynamic finite element model used in the study of reactor vessel dynamic response effects on crack arrest during a PTS transient.

high-straining rates at the crack tip and by so doing reduce the stress-intensity factor required for crack reinitiation. The basic question relative to the utility of dynamic crack arrest relates to the stability of the arrested crack when exposed to the combined effects of dynamic overshoot loading and dynamic degradation of the material fracture toughness (K_{Id}).

Lower bound (-2σ) estimates of K_{Id} for the straining rates shown in Fig. 11 were obtained using the data of Ref. 29 to interpolate between the lower-bound K_{Ic} and K_{Ia} curves of Ref. 30. At the $T-RT_{NDT}$ value appropriate to the arrested crack depth, the estimated dynamic crack-initiation toughness K_{Id} was obtained as $263 \text{ MPa}\sqrt{\text{m}}$ ($240 \text{ ksi}\sqrt{\text{in.}}$). The post-arrest K_{Id} curve in Fig. 11 is seen to reach the critical K_{Id} value at approximately 20 ms following the first arrest, thereby triggering reinitiation of the crack. The reinitiated crack finally arrests at a depth of 10.9 cm (4.3 in.).

Crack arrest depths predicted by the static and dynamic-crack-arrest analysis models evaluated are shown in Fig. 12. The static equilibrium model²¹ of OCA-P is seen to give a slightly conservative crack arrest depth prediction when compared with the dynamic-analysis prediction with crack reinitiation included.

Fig.11 Time history of the crack-tip stress intensity factor following crack initiation showing the effect of reinitiation of the the crack after an initial arrest.

It will be evident from the above discussion that the current analyses provide what is probably a lower bound estimate of the influence of vessel dynamic response effects on crack arrest. The plane strain analysis model of Fig. 10 incorporates the assumption of an infinite flow length. Prior analyses^{31,32} have shown that the stress intensity factor at the deepest point of a semielliptical surface crack remains less than that for an infinite flaw even when the flaw surface dimension becomes very long. The stress-intensity-factor time history given in Fig. 11 for the period following the initial crack arrest will therefore be conservative for flaws of finite length. Incorporation of finite-length-flaw stress intensity factors into the analysis together with a statistical rather than a lower bound representation of K_{Id} would preclude crack reinitiation in many cases. The initial crack arrest would then become the final arrest.

The analysis results of Fig. 12 shows that without reinitiation, the crack depth to vessel-wall-thickness ratio (a/w) at arrest would be 0.36 rather than the value of 0.5 predicted by the OCA-P static equilibrium model. This difference could be very important in a probabilistic fracture mechanics analysis since, beyond a critical depth, the arrested cleavage crack becomes unstable due to the onset of ductile tearing. This effect is illustrated in the critical-crack-depth curves and tearing-instability curves of Fig. 13, which are reproduced from Ref. 33. The OCA-P static-equilibrium analysis (point 5) and the dynamic analysis with crack reinitiation (point 4) both predict crack arrest at depths where ductile tearing would cause an infinitely long arrested cleavage crack to propagate to failure. In the event cleavage crack reinitiation does not occur however, the crack would arrest at a depth where it would remain stable, provided that the vessel does not contain low-upper-shelf material.

Fig.12 Crack arrest depths predicted by the static and dynamic analysis models.

Conclusions

Preliminary results from the shallow-flaw fracture-toughness testing of A533 B Class 1 material confirm the existence of an enhanced shallow flaw fracture toughness effect for this material. This property may decrease the significance to pressure vessel integrity of the shallow flaws which are both most numerous and most difficult to detect.

Studies of the potential effect of out-of-plane biaxial stresses on fracture toughness have been inconclusive to this point. Biaxial fracture toughness tests, with specimens of sufficient size to permit retention of elastic conditions in the unbroken ligament up to the point of fracture, are required to provide a basis for the further development and validation of out-of-plane biaxial fracture models.

Reactor vessel dynamic response effects during crack propagation may have a significant influence on the number of stable crack arrests predicted in a probabilistic PTS analysis. Finite flaw length effects are also important with regard to the arrest and subsequent stability of propagating cracks. Additional dynamic crack initiation toughness (K_{ID}) data for A533 B Class 1 material will be required to determine the magnitude of this effect.

Fig.13 Results from the dynamic analysis indicate that, if reinitiation occurs following the initial arrest, then the second arrest will occur at a depth where the crack will subsequently become unstable due to ductile tearing.

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Heavy-Section Steel Technology Program Recent Developments in Crack Initiation and Arrest Research*

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Oak Ridge National Laboratory

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V/91M-575C ETD

Ongoing HSST Program Research Aims to Generate Crack Initiation and Arrest Toughness Data Reflecting the Unique Combination of Conditions Existing During a PTS Event

- Shallow flaw fracture toughness
- Out-of-plane biaxial loading effects on crack-tip constraint
- Dynamic crack arrest
- Interim conclusions

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SHALLOW FLAW FRACTURE TOUGHNESS

V91M-541C ETD

102-mm (4-in.) Deep Test Beams are Used to Permit Testing of Prototypical Flaw Depths

ORNL-PHOTO 8024-91C

Instrumentation Applied to a 102-mm Square Shallow Flaw Test Beam

ORNL-PHOTO 8410-91A

Preliminary Results Confirm Enhanced Fracture Toughness for Shallow Flaws in A533-B Class 1 Material

OUT-OF-PLANE BIAXIAL LOADING CRACK-TIP CONSTRAINT EFFECTS

V/91M-542C ETD

Pressurized Thermal Shock Loading Produces Biaxial Stresses

One of the Biaxial Stresses Produced by PTS Loading is Aligned with the Crack Tip

A Crack Tip Micromechanical Model Predicts Little Effect of Out-of-Plane Strain on Fracture Toughness

A Dual Parameter J-Acr Fracture Toughness Correlation Model Predicts a 12% Reduction in K_{IC} for a 0.114% Out-of-Plane Strain

Modified Inglis Equations Provide a Basis for Assessing the Effect of Out-of-Plane Stresses on Constraint at the Plastic Zone Boundary

$$\sigma_H = \frac{\sigma_x + \sigma_y + \sigma_z}{3}$$

$$\sigma_H = \frac{1}{\sqrt{2}} [(\sigma_x - \sigma_y)^2 + (\sigma_y - \sigma_z)^2 + (\sigma_z - \sigma_x)^2]^{1/2}$$

$$\text{CONSTRAINT PARAMETER} = \sigma_H / \sigma_e$$

Modified Inglis Equations Predict a Significant Increase in the σ_H/σ_e Ratio at the Plastic Zone Radius for Biaxial Out-of-Plane Loading

A Relationship Developed by V. Weiss at Syracuse University Predicts Substantial Reduction in Fracture Ductility as the Stress Field Constraint Factor Increases

Plane Stress and Plane Strain Fracture Toughness Data Have Been Produced using A533-B Specimens With Varying Widths

ORNL-PHOTO 10398-91

Adaptation of the Weiss Equation for a Fracture-Strain-Dependent Estimate of K_c Provides an Indication of the Possible Magnitude of Out-of-Plane Stresses on Fracture Toughness

$$\frac{(K_c)_{NPS}}{(K_{Ic})_{PS}} = \frac{\left[\sigma_y \sqrt{\left(\frac{\bar{\epsilon}_F, \alpha\beta}{\bar{\epsilon}_y} \right)^{n+1} - 1} \right]_{NPS}}{\left[\sigma_y \sqrt{\left(\frac{\bar{\epsilon}_F, \alpha\beta}{\bar{\epsilon}_y} \right)^{n+1} - 1} \right]_{PS}}$$

$$n = 0.31$$

CONSTRAINT CONDITIONS	σ_x/σ_{ys}	σ_y/σ_{ys}	σ_z/σ_{ys}	$\frac{\bar{\epsilon}_F, \alpha\beta}{\epsilon_{TF}}$	$\frac{(K_c)_{NPS}}{(K_{Ic})_{PS}}$
PLANE STRESS	0.64	1.27	0.11	10.41	4.2
PLANE STRAIN	1.79	2.41	1.26	0.43	1.0
BIAXIAL	3.35	3.98	2.82	0.06	0.47

**Fracture Surfaces From Shallow and Deep Flaw Specimens
Tested at $T\text{-RT}_{NDT} = 17^{\circ}\text{C}$**

ORNL-PHOTO 8408-91A

Microscopic Examination of the Fatigue Crack Front Reveals Irregularities Which Could Inhibit the Buildup of Hydrostatic Stress

Large Scale Biaxial Fracture Tests are Required to Resolve the Issue of Out-of-Plane Biaxial Stress Effects on Fracture Toughness Since Predictions are Model Dependent

FRACTURE MODEL	CONTROLLING PARAMETER	OUT-OF-PLANE FAR-FIELD STRAIN	ESTIMATED ΔK_{IC}
CRACK TIP – MICROMECHANICAL	FRACTURE DUCTILITY	$\epsilon_y^I = + \cdot 2\%$	-2%
CRACK TIP – DUAL PARAMETER (J - $A_C R$)	CRITICAL STRESS – AREA COMBINATION	$\epsilon_y^I = + \cdot 114\%$	-12%
PLASTIC ZONE BOUNDARY: INGLIS-WEISS	FRACTURE DUCTILITY	$\epsilon_y^I = + \cdot 18\%$	-53%

The 100 MN Biaxial Test Facility at AEA Technology (U.K.)
May be Used for the HSST Program Beyond-Plane-Strain
Constraint Effects Fracture Toughness Tests

ORNL-DWG 91-3218C ETD

DYNAMIC CRACK ARREST

V/91M-546C ETD

**A 180° 2-D Plane Strain Model Was Used
in the Evaluation of Vessel Dynamic
Response Effects on Crack Arrest**

Time History of the Crack-Tip Stress Intensity Factor Following Crack Initiation

Crack Arrest Depths Predicted by the Static and Dynamic Analysis Models

Vessel Dynamic Response Effects Could Arrest the Crack at a Shallower Depth If No Reinitiation Occurs

Interim Conclusions

- Preliminary test data indicate a significant increase in fracture toughness for A533-B, Class 1 material at temperatures in the lower transition range
- Biaxial fracture toughness tests on large scale specimens are required to provide the data necessary to develop and validate out-of-plane biaxial constraint effects fracture modes
- Dynamic reactor vessel response effects following crack initiation may reduce the amount of crack penetration, if crack reinitiation does not occur
- Additional dynamic fracture toughness data will be required to support a probabilistic analysis of crack reinitiation

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