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Potential Change in Flow Geometry of an Initially Shallow Finite-Length Surface Flaw During a Pressurized-Thermal-Shock Transient

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Abstract

This study presents preliminary estimates on whether an initially shallow, axially oriented, inner-surface finite-length flaw in a PWR-RPV would tend to elongate in the axial direction and/or deepen into the wall of the vessel during a postulated PTS transient. Results obtained for a family of modified elliptical flaws based on the assumptions of: (1) linear-elastic material response, and (2) cladding with same toughness as the base metal, indicate that a nearly semicircular flaw would likely propagate in the axial direction followed by propagation into the wall of the vessel. These results correspond to initiation within the fracture-toughness lower-shelf range. For the lower-transition range, there

will be a greater tendency for extensive axial propagation and for axial as opposed to radial propagation. The sensitivity of the numerical results and conclusions to the following three analysis assumptions are evaluated: (1) reference flaw geometry along the entire crack front and especially within the cladding region, (2) linear-elastic vs elastic-plastic description of material response, and (3) base-material-only vs bimaterial cladding-base vessel-model assumption. The sensitivity evaluation indicates that the analysis results are very sensitive to the above assumptions. This report is designated HSST Report No. 139.

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1 Introduction

Regulatory Guide 1.154¹ accepts the integrated pressurized-thermal-shock (IPTS) methodology included in Refs. 2, 3, and 4 as an appropriate means for evaluating the integrity of nuclear reactor pressure vessels (RPVs) under pressurized-thermal-shock (PTS) loading conditions. All flaws included in the probabilistic fracture mechanics portion of the IPTS methodology are surface flaws oriented in either a longitudinal or circumferential direction, and all flaws with depths less than ~40 mm (~1.57 in.) are assumed to be effectively infinitely long [two dimensional (2-D)] insofar as the calculation of the stress intensity factor (K_I) is concerned. Three-dimensional (3-D) effects are included for deeper flaws. These particular assumptions regarding flaw length were made because calculations performed for PTS-related confirmatory thermal-shock experiments⁵ indicated that, at least in the absence of cladding, a semicircular short surface flaw had a greater potential for initiation (onset of propagation) than a 2-D surface flaw of the same depth, and, once initiated, the short flaw would propagate in surface length to effectively become a 2-D flaw. This assumption was confirmed by the thermal-shock experiments.⁵ In other words, the existence of a short flaw resulted in the generation of a 2-D flaw before a 2-D flaw of the same depth could have initiated and propagated radially. Under these circumstances there was no need in an IPTS analysis to perform calculations for the short flaw.

Similar comparison calculations were made for PWR vessels under large-break loss-of-coolant accident loading conditions.⁶ For the very limited number of cases considered, and contrary to the results obtained for the previously mentioned thermal-shock experiments, these results indicated that the short flaw had a somewhat less potential than a 2-D flaw for initiation.

To avoid a nonconservative approach with regard to "initial" flaw length and still have an analytically manageable probabilistic fracture-mechanics model for the IPTS methodology, the 2-D initial flaw was adopted in Ref. 1.

At the time the comparison calculations in Refs. 5 and 6 were made, the necessary 3-D fracture analyses were very expensive and time consuming and, thus, were not performed extensively. Furthermore, there was a conservative way out of the dilemma (the assumption of a 2-D initial flaw). More recently, however, incentives for a more accurate evaluation have developed, and the practical numerical means for performing the more accurate evaluation have become available. A preliminary step toward achieving a more accurate evaluation is the subject of this study.

The objective of this study is to provide preliminary estimates on the potential change in flaw geometry of an initially shallow, axially oriented, inner-surface finite-length flaw in a PWR-RPV during the course of a postulated PTS transient. These estimates have been obtained based on a series of scoping analyses using the finite-element method. Specifically, the question being addressed is whether a shallow finite-length surface flaw would tend to elongate in the axial direction and/or deepen into the wall of the vessel during the transient. Results from this study are expected to contribute toward an assessment of the degree of conservatism associated with the use of 2-D axial flaws in a probabilistic fracture mechanics assessment of an RPV.

In the first part of the study, estimates have been obtained in accordance with the assumptions of conventional linear-elastic fracture-mechanics (LEFM) concepts.⁷ The thermoelastic properties of the cladding and base material are explicitly considered. The flaw geometry corresponds to a family of modified elliptical flaws characterized by the flaw's half-length to maximum depth (aspect ratio). Numerical results are presented for values of the aspect ratio of 1:1 and 80:1. Requirements for numerical convergence of the finite-element results for this family of flaw geometries are examined.

In the second part of this study, the sensitivity of the numerical results and conclusions obtained in the first part of this study to some of the analysis assumptions are evaluated.⁸ Three specific issues related to the analysis assumptions are considered. The first issue concerns the influence of reference flaw geometry along the entire crack front, and especially within the cladding region, on the conclusions reported previously. The second issue concerns the influence of linear-elastic vs elastic-plastic description of material response on the analysis outcome. The third issue concerns the influence of modeling the RPV as a base-material-only or as a bimaterial cladding-base metal cylinder on the analysis outcome.

2 Method of Analysis

2.1 Basic Concepts and Methodology

Estimates of the potential change in flaw geometry during the PTS transient begin with a comparison of the mode I stress intensity factor (K_I) values along the crack front of the initial shallow flaw with the static fracture toughness (K_{Ic}) of the vessel material. The K_I distributions are obtained using the finite-element method in accordance with the assumptions of conventional LEFM concepts. The static fracture toughness is assumed to vary through the wall of the vessel as a function of the through-wall fluence distribution and the through-wall temperature profiles during the transient.

Crack initiation is assumed to be possible when K_I exceeds K_{Ic} at any point along the initial crack front. It is then assumed that crack initiation at the critical location introduces dynamic loading effects along the remainder of the crack front. Consequently, the dynamic fracture toughness, K_{Id} , of the vessel material is assumed to determine that portion of the crack front that participates in crack initiation.

While not explicitly considered in this study, extension of the present methodology beyond the initial initiation can be considered whereby a stable flaw geometry subsequent to initiation is assumed possible when K_I along the initiated crack front falls to the crack-arrest toughness (K_{Ia}). The flaw is then assumed to maintain its new geometry until a later time in the transient when K_I again exceeds K_{Ic} somewhere along the new crack front, at which time the flaw geometry will again change in accordance with the interpretation method outlined above.

2.2 Pressurized-Thermal-Shock Transient

The PTS transient considered in this study is indicated in Fig. 1, which simulates the occurrence of a small-break loss-of-coolant accident (SBLOCA). The vessel is assumed to operate at 268°C (515°F) prior to the transient. The cooling water temperature is also at 268°C (515°F) prior to the transient.

Figure 1 Definition of pressurized-thermal-shock transient

2.3 Fracture Toughness

The static fracture toughness (K_{Ic}) as a function of $T-RT_{NDT}$ for the vessel base material is identified with the lower-bound (IPTS-mean minus two standard deviations) ASME curve.⁹ ΔRT_{NDT} is determined from Reg. Guide 1.99, Rev. 2 (base metal)¹⁰ based on an assumed chemistry of 0.29% Cu and 0.6% Ni, vessel inner-surface fluence of 1.7×10^{19} n/cm² ($E > 1$ MeV), and a low-temperature-irradiation additional RT_{NDT} shift of 27.8°C (50°F).^{*} The initial RT_{NDT} is assumed to be -1.1°C (30°F). The dynamic fracture toughness (K_{Id}) for the vessel base material is conservatively represented by K_{Ia} .

In addition, it is assumed that the clad-base interface is fully bonded and that the interface, along with the regions adjoining the interface such as the heat-affected

^{*} A value of 27.8°C (50°F) implies that the effective operating temperature over the history of the vessel is 260°C (500°F).

zones, share the fracture toughness of the base material. The cladding is also conservatively assumed to share the fracture toughness of the base material. The findings of this study, when the cladding toughness is assumed to be higher than that of the base material, are addressed subsequent to the presentation of analysis results.

2.4 Finite-Element Model of Vessel and Initial Flaw Geometry

A finite-element model of the beltline region of an RPV is generated with the ORNL program ORMGEN as indicated in Fig. 2(a).¹¹ An initially shallow, axially oriented, inner-surface finite-length flaw is assumed to exist within this region as indicated in Fig. 2(b). From symmetry considerations only one-fourth of the vessel is modeled. Quadratic elements are used throughout the model. The vessel is modeled with a 2.769-mm thick (0.109-in.) stainless steel cladding on the inner surface. Details on the vessel geometry along with the thermoelastic properties of the cladding and base material are listed in Table 1.

In the first part of this study the initial flaw geometry corresponds to a nearly semicircular flaw with a maximum flaw depth of 6.35 mm (0.25 in.), as indicated in Figs. 2(b) and 3. The initial flaw geometry is constructed based on a family of flaw geometries herein denoted as modified ellipses. This family of flaw geometries is constructed with a crack front that is elliptical in the base material and is a straight line perpendicular to the vessel's inner-surface in the cladding region.

Figure 2(a) Finite-element model of reactor pressure vessel with an initially shallow, axially oriented inner-surface flaw

Figure 2(b) Close-up of region of vessel containing flaw

Table 1. Vessel geometry and thermoelastic properties of the cladding and base material

Vessel Geometry:

Inner vessel radius = 138 cm (54.5 in.)
Wall thickness = 20 cm (7.875 in.)
Vessel length = 305 cm (120 in.)

Cladding Thickness = 0.277 cm (0.109 in.)

Cladding Thermal-Elastic Material Properties:*

Modulus of elasticity (E) = 186 GPa
(27,000 ksi)
Poisson's ratio (ν) = 0.3
Thermal expansion coefficient (α_{clad}) =
1.8E-5 /°C (9.9E-6 /°F)
Thermal conductivity (k) = 17 W/m·°C
(10 BTU/h·ft·°F)
Specific heat (c_p) = 500 J/kg·°C
(0.12 BTU/lb·°F)
Density (ρ) = 7820 kg/m³ (488 lb/ft³)

Base Metal Thermal-Elastic Material Properties:*

Modulus of elasticity (E) = 193 GPa
(28,000 ksi)
Poisson's ratio (ν) = 0.3
Thermal expansion coefficient (α_{base}) = 1.41E-5 /°C
(7.85E-6 /°F)
Thermal conductivity (k) = 42 W/m·°C
(24 BTU/h·ft·°F)
Specific heat (c_p) = 500 J/kg·°C
(0.12 BTU/lb·°F)
Density (ρ) = 7820 kg/m³ (488 lb/ft³)

Coefficient of Convective Heat Transfer =
2860 W/m²·°C (504 BTU/h·ft²·°F)

*No temperature dependence of material properties included in analyses.

Figure 3 Detail of nearly semicircular inner-surface flaw constructed based on the modified elliptical flaw geometry with aspect ratio 1:1

A maximum flaw depth of 6.35 mm (0.25 in.) is chosen for the initial flaw geometry based on findings from probabilistic fracture mechanics safety assessments of RPVs subject to a postulated pressurized-thermal-shock transient.^{2,4} As discussed in the Introduction, current probabilistic analyses that involve a "shallow" flaw depth assume a 2-D plane strain model of both the vessel and flaw geometry, in that both the vessel and flaw are assumed to be infinite in length in the axial direction of the vessel. Available analysis results suggest that, for SBLOCA transients, the majority of surface flaws causing nonarresting fractures have depths close to 6.35 mm (0.25 in.).

2.5 Solution Validation and Mesh Refinement

The finite-element calculations are carried out using the computer program ABAQUS.¹³ Values of the mode I stress intensity factor (K_I) along the crack front of a given model are determined from the associated J-integral values using the plane strain conversion formula and assuming a value of the Young's modulus and Poisson's ratio for the base material. The J-integral values are determined using the J-integral option within ABAQUS.

A limited number of comparison calculations have been performed using the computer program ADINA.¹⁴ In the case of ADINA, the J-integral values are determined using the ORNL post-processing program ORVIRT.¹⁵ Relatively good agreement in calculated K_I values along the crack front is found with these two programs.

Alternative (approximate) validation of the K_I value, at both the deepest point of the crack and at the inner surface of the vessel, has been performed based on available solutions for a shallow elliptical surface flaw in a plate. Results from this validation are also in good agreement with the finite element results as discussed in Appendix A.

The effects of mesh refinement for modeling (1) the behavior of the cladding and (2) the global behavior of the vessel and its effects on the calculated K_I values are examined in Appendix B. It is observed that for the analysis conditions examined in this study, sensitivity of the computed K_I values to mesh-refinement effects in the cladding region is minimal but is significant for such effects in the circumferential direction of the vessel.

In this study, in order to keep the computational requirements manageable, finite-element results have been obtained based on models of the vessel with either (1) one element in the cladding region and 12 elements in the circumferential direction or (2) two elements in the cladding region and ten elements in the circumferential direction. Since only minor differences in analysis results exist between these two types of model, no attempt is made to identify the type of finite-element model associated with the analysis results to be presented.

3 Analysis Results Based on Modified Elliptical Flaw Geometry

3.1 Distribution of K_{Ic}

Variation of the static fracture toughness (K_{Ic}) (IPTS-mean minus two standard deviations) through the vessel wall up to a depth of 6.35 mm (0.25 in.) is indicated in Fig. 4 as a function of transient time from 10 to 25 min. Note that for transient times beyond ~20 min, the distribution of K_{Ic} is essentially constant over this range of wall depth.

Figure 4 Distribution of K_{Ic} through the vessel wall up to a depth of 6.35 mm (0.25 in.) as a function of transient time (t) in minutes

3.2 Crack Initiation

Variation of the applied K_I values along the crack front of the nearly semicircular flaw (Fig. 3) is indicated in Fig. 5 as a function of transient times from 0.1 min to 25 min. Note that the K_I values achieve their maximum values at ~20 min into the transient. Included in Fig. 5 are the K_{Ic} distributions through the vessel wall at four transient times of interest.

Results shown in Fig. 5 indicate that K_{Ic} is exceeded at the location where the crack front intersects the inner surface of the vessel at ~20 to 25 min into the transient. For simplicity, it is assumed that initiation first takes place at 25 min into the transient with the K_{Ic} distribu-

tion approximately constant at $K_{Ic} = 44 \text{ MPa}\sqrt{\text{m}}$ (40 $\text{ksi}\sqrt{\text{in.}}$) up to a wall depth of 6.35 mm (0.25 in.). Note that the K_I value at the inner surface of the vessel cannot be strictly evaluated using the present finite element formulation. However, one may interpret the above result to imply that initiation will occur near the free surface.

Figure 5 Distribution of applied K_I along the crack front of the nearly semicircular modified elliptical flaw as a function of transient time (t) in minutes, along with K_{Ic} distributions relevant to consideration of initiation

3.3 Potential for Flaw Stability After Initiation

The ratio K_{Ia}/K_{Ic} corresponding to $K_{Ic} = 44 \text{ MPa}\sqrt{\text{m}}$ (40 $\text{ksi}\sqrt{\text{in.}}$) is 0.8, so that K_{Ia} is estimated as 35.2 $\text{MPa}\sqrt{\text{m}}$ (40 $\text{ksi}\sqrt{\text{in.}}$). The initial nearly semicircular flaw will first elongate in the axial direction. Strictly speaking, only that portion of the crack front within ~2.54 mm (0.1 in.) of the inner surface, which is confined within the cladding, would extend at this time in the transient.

For the purpose of ascertaining whether the initiated portion of the flaw would arrest and result in a stable flaw, the K_I value for an elongated surface flaw with a depth of 6.35 mm (0.25 in.) and a half-length of 508 mm (20 in.) (aspect ratio of 80:1) at 25 min into the transient is indicated in Fig. 6, along with the distribution at initiation for the nearly semicircular flaw (1:1). It is observed that at 25 min the value of K_I along most of the elongated crack front is above $K_{Ia} = 35.2$

MPa $\sqrt{m}$ (32 ksi $\sqrt{in.}$).

Figure 6 Distribution of applied K_I along the crack front for two members (1:1, 80:1) of the modified elliptical family of surface flaws of a depth of 6.35 mm (0.25 in.) at 25 min into the transient

Note that the K_I values at the inner surface of the vessel, for the modified elliptical family of elongated flaws with a depth of 6.35 mm (0.25 in.) considered in this study, do not appear to decrease significantly with increase in axial flaw dimension. It appears reasonable to expect that any increase in depth of the flaw would elevate the applied K_I value near the inner surface of the vessel. Therefore, the implication is that the nearly semicircular flaw would extend along the entire length of the vessel during the transient.

In the absence of K_I values for intermediate flaw geometries, it can only be surmised that, for the particular analysis conditions assumed in this study, the nearly semicircular flaw will initially extend in the axial direction and eventually be followed by propagation into the wall of the vessel. Crack arrest in the axial direction within the belt-line region is not

expected. Without additional calculations, it is not possible to conclude whether the flaw would arrest in the depth direction.

It is appropriate at this point of the discussion to establish connections between the present results for 3-D, finite-length flaws with a depth of 6.35 mm (0.25 in.) and 2-D results based on the assumption of an axially infinite surface flaw of the same depth. The value of K_I for the associated 2-D flaw with a depth of 6.35 mm (0.25 in.) at 20 min into the transient, as determined from OCA-P,¹² is ~55 MPa $\sqrt{m}$ (50 ksi $\sqrt{in.}$). Consequently, the initially shallow, nearly semicircular (1:1) 3-D, finite-length flaw has less potential for initiation than a 2-D flaw of the same depth. However, since the finite-length flaw is expected to extend along the entire length of the vessel during the transient, the evident conservatism associated with the use of a 2-D flaw geometry is of no consequence in the present *deterministic* evaluation. On the other hand, without additional calculations, it is not possible to quantify the degree of conservatism associated with the use of a 2-D flaw geometry in a *probabilistic* evaluation in which the toughness of the base material, among other parameters, is treated in a statistical fashion. It is noted that in a probabilistic analysis involving a SBLOCA transient, the majority of calculated initiations associated with a flaw depth of approximately 6.35 mm (0.25 in.) lead to nonarresting fractures of the vessel.

The above discussion on the relative conservatism associated with a 2-D vs 3-D flaw geometry does not include the possible influence of warm prestress as a function of the flaw geometry.¹⁶ Based on the K_{Ic} results presented in Fig. 5, a 2-D flaw is likely to initiate at a slightly earlier time during the transient as compared to the 3-D flaw and is of little consequence in the present discussion. However, it is conceivable that for some combination of PTS-transient conditions a 2-D flaw would be predicted to initiate ($K_I = K_{Ic}$) prior to attaining a maximum value in K_I , whereas a 3-D flaw would be predicted to initiate ($K_I = K_{Ic}$) subsequent to the attainment of a maximum value in K_I . Under these circumstances, for example, incorporation of warm prestress effects would result in the prediction of crack initiation for the 2-D flaw geometry but not for the 3-D flaw geometry. Note that current guidelines for performing PTS safety analysis do not permit the inclusion of warm prestress effects.¹

3.4 Interactions Between K_I and Fracture Toughness Gradient

Within the context of the interpretation method described above, it is clear that whether a flaw will tend to extend exclusively in either the axial or depth direction or a combination of both directions is strongly dependent on the values of K_I/K_{Ic} , K_I/K_{Id} , and K_I/K_{Ia} , along the crack front and the value of K_{Ic}/K_{Ia} initially and as the crack front extends. This observation can be illustrated by reconsidering the nearly semicircular flaw just analyzed. The K_I value is highest near the inner surface within the cladding, which would tend to cause the flaw to elongate initially. However, as the flaw progressively elongates in the axial direction, the K_I value at the deepest point would increase while the value near the inner surface would decrease, with the eventual outcome that the K_I value at the deepest point becomes the largest value along the crack front. This is the case for the elongated 80:1 flaw. Note that for the elongated 80:1 flaw geometry the K_I value at the deepest point of the crack is only slightly larger than the K_I value near the inner surface.

The tendency for extensive axial extension without arrest increases with increasing K_{Ic}/K_{Ia} , and the tendency for axial as opposed to radial extension increases with increasing positive radial gradient in toughness. As indicated in Fig. 4, the toughness gradient is quite flat for the problem evaluated in this report. Thus, for a case involving a steeper toughness gradient, the relative potential for axial extension would be even greater than observed in this study. For the fluence-related embrittlement, the PTS transient, and the time in the transient (25 min) selected for this study, the vessel temperatures in the vicinity of the shallow flaw correspond to the lower shelf of the toughness curve. This explains the flat toughness distribution in Fig. 4. For a case involving less embrittlement, the toughness gradient would be steeper and K_{Ic}/K_{Ia} would be larger; thus there would be a more extensive axial extension of the flaw and less potential for radial extension, if initiation took place. Of course, the less the embrittlement, the lower the potential for initiation.

3.5 Implications of Higher Toughness in Cladding Than in Base Material

Note that estimates of potential flaw-geometry change are very sensitive to assumptions regarding the fracture response of the cladding. In this study, it has been assumed that the cladding shares the toughness of the base material. A more realistic view may be that the cladding is tougher than the base material.

Available experimental data for the irradiated ductile-tearing toughness of a stainless steel cladding material¹⁷ indicate that, at 25 min into the present transient, which corresponds to a vessel inner-surface temperature of 113°C (236°F), the cladding toughness is $\sim 143 \text{ MPa}\sqrt{\text{m}}$ ($130 \text{ ksi}\sqrt{\text{in.}}$). [The value of $143 \text{ MPa}\sqrt{\text{m}}$ ($130 \text{ ksi}\sqrt{\text{in.}}$) corresponds to a fluence level of $2.41 \times 10^{19} \text{ n/cm}^2$ ($E > 1 \text{ MeV}$), which is higher than the fluence assumed in this study.] If one incorporates this observation into the interpretation method, the results presented in Fig. 5 suggest that the nearly semicircular flaw would not initiate during the course of the postulated PTS transient. This observation is based on the fact that, during the course of the transient, K_{Ic} is exceeded only within a narrow region of the cladding even at 20 min into the transient, which corresponds approximately to the time of maximum loading. Beyond 20 min both the applied K_I and K_{Ic} fall with time, and it is not anticipated that K_I would exceed K_{Ic} thereafter for the conditions considered in this study.

The above observation is significant in view of the fact that, at present, analysis results from probabilistic fracture mechanics safety assessments of RPVs subjected to a postulated SBLOCA transient show that a significant portion of the predicted vessel failures are due to the initiation and propagation of shallow 2-D flaws with a depth of $\leq 6.35 \text{ mm}$ (0.25 in.). As discussed in the Introduction, implicit in the use of 2-D flaws is the assumption that a shallow inner-surface finite-length flaw would extend in the axial direction of the vessel prior to its possible penetration into the wall of the vessel. The assumption of cladding toughness higher than that of the base material thus appears to represent one avenue through which the tendency for a 3-D, finite-length surface flaw to convert to an essentially 2-D surface flaw could be inhibited. The present findings thus suggest that a significant level of conservatism may be associated with the use of 2-D surface flaw geometries and the assumption of identical toughnesses for both the base and cladding material.

The possibility of subclad crack extension, while not explicitly considered in this study, represent an alternate avenue through which the tendency for a 3-D surface flaw to convert to a 2-D surface flaw could be inhibited. Indeed, subclad crack extension in the form of crack tunneling (both axial lengthening and radial deepening of flaw geometry) have been observed experimentally.¹⁸ In Ref. 18, it is reported that initially part-through finite-length surface and subclad cracks extend beneath the cladding to become effectively 2-D subclad cracks under thermal-shock loading.

Finally, it is emphasized that the sensitivity of the present findings to variability in the toughness of the base material has not been investigated. This study models the base material with only a single value of ΔRT_{NDT} . It is anticipated that a full investigation that incorporates, among other parameters, the variability in toughness of the base material, would be able to more realistically identify the population of finite-length flaws that would not propagate to become 2-D flaws.

4 Sensitivity of Numerical Results to Analysis Assumptions

In the second part of this study, the sensitivity of the numerical results and conclusions obtained in the first part of the study to some of the analysis assumptions are evaluated. Three specific issues related to the analysis assumptions are considered. The first issue concerns the influence of reference flaw geometry along the entire crack front, and especially within the cladding region, on the conclusions reported previously. That is, to what extent were the results from the first part of this study dependent on the specific family of flaw geometries considered, all of which can be characterized by nominally identical aspect ratios? The second issue concerns the influence of linear-elastic vs elastic-plastic description of material response on the analysis outcome. This is important since existing computer programs for IPTS-type analysis assume linear-elastic material response only. The third issue concerns the influence of modeling the RPV as a base-material-only or as a bimaterial cladding-base cylinder. This is important since available information on the thermomechanical properties of the cladding material is relatively scarce as compared with the base material, especially under irradiated conditions.

As discussed in Sect. 3, maximum loading of the flawed cylinder, in terms of the magnitude of the applied stress intensity factor K_I , occurs at ~20 min into the transient. In subsequent discussions, analysis results are presented at 25 min into the transient.

4.1 Effects of Reference Flaw Geometry

Three families of crack-front geometries have been considered: (1) modified ellipse: this is the flaw geometry considered in the first part of the study, in which the crack-front is elliptical in the base material but is straight and perpendicular to the vessel's inner surface in the cladding region; (2) canoe: the crack-front in the base material is essentially a straight line parallel to the vessel's longitudinal axis with two semi-circular regions near the ends, intersecting the clad/base interface at 90 degrees and with the crack front in the cladding being straight and perpendicular to the vessel's inner surface; (3) ellipse: the crack-front is elliptical in shape throughout the base and cladding material.

Comparison of the various families of flaw geometries is indicated in Figs. 7(a) and (b) for the case of a nominal (80:1) aspect ratio. In Fig. 7(a), the vertical and horizontal axes are drawn to vastly different scales to emphasize the differences in crack-front geometries. In Fig. 7(b), crack-front geometries are redrawn with more similar vertical and horizontal scales to

emphasize the observation that the differences in flaw geometries in Fig. 7(a) are minor in comparison with the overall geometry of the nominal flaw shape.

Figure 7(a) Comparison of the various families of flaw geometry for the case of a nominal (80:1) aspect ratio – (1) modified ellipse, (2) canoe, (3) ellipse. The vertical and horizontal axes are drawn to vastly different scales to emphasize the differences in crack-front geometries

Figure 7(b) Crack-front geometries redrawn with more similar vertical and horizontal scales to emphasize the observation that the differences in flaw geometries in (a) are minor in comparison with the overall geometry of the nominal flaw shape

Effects of reference flaw geometry on the computed K_I values are isolated via a series of LEFM scoping calculations in which the vessel is assumed to be made of base-material only. In Fig. 8, results are presented for the modified elliptical and elliptical flaw geometry based on a common aspect ratio of 1:1. The modified ellipse is thus nearly semicircular while the ellipse is exactly semicircular in shape. In Fig. 9, results are presented for all three flaw geometries based on a common aspect ratio of 80:1.

Figure 8 Effects of reference flaw geometry on the computed K_I values are minor for the case where aspect ratio is 1:1. Results are 25 min into the transient

Analysis results indicated in Fig. 8 suggest minor effects of reference flaw geometry on the computed K_I values for the case where the aspect ratio is 1:1. On the other hand, analysis results indicated in Fig. 9 suggest that the crack-front geometry within the cladding can significantly influence the computed K_I values along the crack front within the cladding region, especially for the case of elongated surface flaws with large aspect ratios. Specifically, for the case where the aspect ratio is 80:1, analysis results indicate that (a) computed K_I values at the deepest point of the crack front agree for all three flaw geometries, (b) computed K_I values along the cladding portion of the crack front near the inner-surface can differ by up to a factor of 5 among the flaw geometries considered, with the maximum value being for the canoe flaw geometry and the minimum value being for the ellipse flaw geometry. Results in Figs. 8 and 9 together appear to suggest that the computed K_I values along the crack front within the cladding region increase with increasing curvature of the crack front in that region.

Figure 9 Effects of reference flaw geometry on the computed K_I values are significant for the case where aspect ratio is 80:1. Results are for 25 min into the transient

Evidently, potential conversion of an initially 3-D shallow surface flaw to an essentially 2-D axial surface flaw geometry due to PTS loading can be substantially enhanced or inhibited based on the actual flaw geometry during the conversion process. It is cautioned that these results do not provide direct information on which flaw geometry is most likely to exist during the course of crack extension. Furthermore, it is emphasized that while the computed K_I values indicated in Fig. 9 are very sensitive to the flaw geometries investigated, the differences in flaw geometries are minor in comparison with the overall geometry of the nominal flaw shape. It thus appears that care must be taken to represent the crack front shape realistically near its ends in order to avoid inaccuracies in the value of K_I there and thus the tendency for axial extension.

4.2 Effects of LEFM vs EPFM Assumptions

Effects of linear-elastic vs elastic-plastic description of material response on the computed K_I values are isolated via a series of scoping calculations involving the modified elliptical flaw geometry at 25 min into the transient. The thermoelastic properties of the base and cladding materials remain as before. In Fig. 10, the true stress-true plastic strain curves adopted for the base and cladding materials are indicated. The cladding material is characterized by a bilinear curve with yield stress (σ_y) = 238 MPa (33.4 ksi) and

ultimate stress (σ_u) = 480 MPa (69.6 ksi). The cladding properties are estimated based on experimental data presented in Ref. 17, which indicate that neither σ_y nor σ_u is very sensitive to fluence levels up to 5×10^{19} n/cm² ($E > 1$ MeV) over the temperature range of 100°C—300°C (212—572°F). The base material is characterized by a multilinear curve with yield stress (σ_y) = 388 MPa (56.3 ksi-√in.) and ultimate stress (σ_u) = 617 MPa (89.4 ksi). The base-material properties correspond to A533B steel plate HSST 13-A at 200°C (392°F).¹⁹ Analysis results based on elastic-plastic material description are obtained using a rate-independent, J_2 (isotropic-hardening) incremental small strain plasticity formulation as implemented in ABAQUS.

Figure 10 True stress—true plastic strain curves for cladding and base material

In Fig. 11, results are presented for the case where the aspect ratio of the modified elliptical flaw geometry is 1:1. The computed K_I values along the cladding portion of the crack front are lower for the case of elastic-plastic material response than the elastic analysis conditions. The opposite trend is seen for the computed K_I values beyond the cladding region.

In Fig. 12, results are presented for the case where the aspect ratio of the modified elliptical flaw geometry is 80:1. Near the inner-surface along the cladding portion of the crack front, the computed K_I values are lower for the case of elastic-plastic material response than the elastic analysis conditions. A similar trend is seen for the computed K_I values along a substantial portion of the crack front within the base material.

Figure 11 Effects of linear-elastic vs elastic-plastic description of material response on the computed K_I values for the case where the aspect ratio of the modified elliptical flaw geometry is 1:1

Figure 12 Effects of linear-elastic vs elastic-plastic description of material response on the computed K_I values for the case where the aspect ratio of the modified elliptical flaw geometry is 80:1

Analysis results in Figs. 11 and 12 thus indicate that the computed K_I values are very sensitive to whether the cladding and base materials are modeled as elastic or elastic-plastic, with the latter analysis assumption being more realistic. Near the inner-surface along the cladding portion of the crack front, the computed K_I values are lower for the case of elastic-plastic than elastic material response. Within the base material, the

relative ordering depends on the aspect ratio of the flaw.

The lower K_I values associated with elastic-plastic conditions, relative to linear-elastic conditions, near the inner-surface along the cladding portion of the crack front can be readily explained in terms of the lower resultant stresses (or tractions) that develop over that region. The lower resultant stresses are due to the imposition of a finite value of the yield stress under elastic-plastic conditions and the fact that the cladding yield stress is substantially lower than that for the base material. Similar observations have been reported in Ref. 20.

Comparison of the results shown in Figs. 11 and 12 suggests that, as the nominal flaw shape in terms of the overall aspect ratio increases, the assumption of elastic-plastic material response leads to the maximum K_I value being located away from the inner-surface of the vessel toward the deepest point of the crack front. Migration of the location of maximum K_I toward the deepest point of the crack front is suggestive of an enhanced tendency toward radial rather than axial crack propagation and, therefore, lessens the likelihood of the conversion of an initially 3-D shallow surface flaw to an essentially 2-D axial flaw geometry.

One aspect concerning the inclusion of plasticity effects in evaluating the propensity for crack initiation that has not been considered in this study is the role of crack-tip constraint as characterized by the Q -stress parameter.²¹⁻²³ Available experimental data for shallow through-thickness surface flaws of depths ~10 mm (0.4 in.) under mechanical three-point-bend loading suggest that enhanced fracture toughness is associated with this shallow-flaw geometry, and that the so-called shallow-flaw toughness effect can be related to the role of the nonsingular hydrostatic Q -stress as a descriptor of crack-tip constraint.^{24,25} Limited analytical results have been obtained for the crack-tip fields associated with a shallow (~10 mm) 2-D surface flaw in a thermally shocked cylinder and in a PWR-RPV during the course of a PTS transient.^{16,24,26,27} These analytical results suggest that an elastic-plastic, J - Q -based description of the crack-driving force provides a more appropriate framework for evaluating the fracture response of a PWR-RPV during a PTS transient than conventional single parameter (K - or J -only) methods under elastic or elastic-plastic conditions.

4.3 Effects of Base-Material-Only vs Cladding-Base Vessel-Model Assumptions

Effects of modeling the RPV as either base-material-only or as a bimaterial cladding-base vessel on the computed K_I values are examined via a series of scoping calculations in this section. Material models of the RPV considered are (1) elastic base and cladding material [bimaterial elastic (Bi-El)], (2) elastic base with cladding material assigned base material properties [base-only elastic (Base-El)], and (3) elastic-plastic base and cladding material [bimaterial elastic-plastic (Bi-Ep)]. Note that for the present analysis conditions, the vessel through-wall temperature profile is minimally affected by the assumption of cladding (stainless steel) or base (A533B steel) material thermal properties. The primary effect of either a base-material-only or a bimaterial assumption in the cladding region concerns the value of the thermal expansion coefficient in the case of an elastic analysis and the additional influence of the stress-strain curve in the case of an elastic-plastic analysis.

In Fig. 13, results are presented for the case where the aspect ratio of the modified elliptical flaw geometry is 1:1. It is observed that assumption of a base-material-only elastic model results in lowest values of the computed K_I values almost everywhere along the crack front. In Fig. 14, results are presented for the case where the aspect ratio of the canoe flaw geometry is 80:1. Again, it is observed that assumption of a base-material-only elastic model results in lowest values of the computed K_I values almost everywhere along the crack front.

From Figs. 13 and 14, the assumption of an elastic base-metal-only model of an RPV, as compared to either an elastic clad-base metal or an elastic-plastic clad-base metal model, is seen to result in lowest computed K_I values almost everywhere along the crack front. Consequently, analysis of the safety margin of an RPV with respect to PTS loading based on a simplified elastic base-metal-only model is nonconservative. It is noted that current guidelines do in fact require that the thermo-mechanical properties of the cladding material be included in a plant-specific PTA analysis.¹

Figure 13 Effects of vessel-model assumptions on the computed K_I values for the case where the aspect ratio of the modified elliptical flaw geometry is 1:1

Figure 14 Effects of vessel-model assumption on the computed K_I values for the case where the aspect ratio of the canoe flaw geometry is 80:1

5 Conclusions

From the first part of this study, based on LEFM assumptions and considering only a family of modified elliptical flaws, the following conclusions are drawn.

- A. Within the context of the interpretation method outlined here, whether a flaw will tend to extend exclusively in either the axial or depth direction or a combination of both directions is strongly dependent on the K_I values along the crack front and the gradient of fracture toughness through the vessel wall at the time of initiation.
- B. If one assumes that the cladding shares the toughness of the base material, preliminary results from this study indicate that for the assumed fracture toughness and initial flaw geometry, the nearly semicircular flaw would likely propagate in the axial direction in an unrestrained manner during the transient followed by propagation into the wall of the vessel. Without additional calculations it is not possible to conclude whether the flaw would arrest in the depth direction. Note that these results correspond to initiation within the lower-shelf fracture toughness temperature range, and that their general validity within the lower-transition temperature range remains to be determined.
- C. A 3-D, nearly semicircular, finite-length flaw has less potential for initiation than a 2-D flaw of the same depth for the conditions examined in this study. However, since the finite-length flaw is still expected to extend along the entire length of the vessel during the transient, the conservatism associated with the use of a 2-D flaw geometry is of no consequence in the present *deterministic* evaluation. However, note that this observation has general applicability only when warm pre-stress effects are not considered. Furthermore, without additional calculations it is not possible to quantify this type of conservatism in a *probabilistic* evaluation.

- D. Available experimental data indicate that the cladding may be tougher than the base material. When this observation is incorporated into the interpretation method, the results from this study suggest that a nearly semicircular flaw would not initiate during the course of the PTS transient for the conditions examined in this study. The assumption of cladding toughness higher than that of the base material thus appears to represent one avenue through which the tendency for 3-D, finite-length flaws to convert to essentially 2-D surface flaws could be inhibited. A related mechanism for inhibiting the conversion to 2-D surface flaws is the formation of subclad flaws.

From the second part of this study, which is focused on various analysis sensitivity issues, the following conclusions are drawn.

- A. Analysis results suggest that the crack-front geometry within the cladding can significantly influence the computed K_I values along the crack front within the cladding region, especially for the case of elongated surface flaws with large aspect ratios. The computed K_I values along the crack front within the cladding region increase with increasing curvature of the crack front in that region.
- B. Analysis results indicate that the computed K_I values are very sensitive to whether the cladding and base materials are modeled as elastic or elastic-plastic, with the latter analysis assumption being more realistic. Near the inner-surface along the cladding portion of the crack front, the computed K_I values are lower for the case of elastic-plastic than elastic material response. Within the base material, the relative ordering depends on the aspect ratio of the flaw.
- C. The assumption of an elastic base-metal-only model of an RPV, as compared to either an elastic clad-base metal or an elastic-plastic clad-base metal model, results in lowest computed K_I values almost everywhere along the crack front. Consequently, analysis of the safety margin of an RPV with respect to PTS loading based on a simplified elastic base-metal-only model is nonconservative.

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Appendix A

Closed-Form Solutions for K_I for a Shallow Elliptical Surface Flaw in a Plate

Essentially closed-form solutions for K_I for a shallow elliptical surface flaw in a plate are available in the literature.^{1-4,*†} These closed-form solutions are applicable under LEFM conditions and require that the distribution of tractions normal to the "crack face" in the usual "cut-and-release" sense be provided as input. In the following discussion, both the thermal analysis and the resultant distribution of tractions are obtained from the computer code OCA-P, such that the closed-form solutions are obtained in a manner completely independent of the finite-element models or the computer code ABAQUS.

In the closed-form calculations indicated in Table A.1, the RPV is modeled as a linear-elastic bimaterial vessel with an elliptical flaw geometry. Values are obtained at both the deepest point of the crack and at the inner surface of the vessel at 25 min into the transient. Both the 1:1 and 80:1 aspect ratios are considered. Validation of the finite-element results with the closed-form solutions is approximate in nature because the "bimaterial/elliptical flaw" combination was not examined as part of the finite-element analysis matrix because of practical limitations in the mesh-generation program. However, the "bimaterial/modified elliptical flaw" and "base-material-only/elliptical flaw" combinations were considered as part of the finite-element analysis matrix. This slightly indirect validation is useful in checking the value of K_I at the deepest point of the crack in Figs. 6, 8, 9, and 11-13 and in confirming the relative ordering of K_I values at the inner surface of the vessel in Fig. 13.

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Table A.1 Closed-form solutions for K_I at two locations along the crack front for the "bimaterial/elliptical flaw" combination

Flaw half-length (mm)	Deepest point K_I (MPa $\sqrt{m}$)	Surface K_I (MPa $\sqrt{m}$)
6.35 (1:1)	29.8 ^{1,2} 30.8 ^{3,4}	44.8 ^{1,2} 41.1 ^{3,4}
508 (80:1)	58.7 ^{1,2} 53.8 ^{3,4}	7.86 ^{1,2} 8.01 ^{3,4}

Appendix B

Effects of Mesh Refinement on K_I Values Along the Crack Front

Circumferential Stiffness of Vessel

Axial flaws are considered in this study. Consequently, accurate calculations of K_I values along the crack front requires that the circumferential stiffness of the vessel be properly modeled. The effects of mesh refinement in the circumferential direction on the calculated K_I values are examined by successively modeling the circumferential extent of the vessel with six, ten and twelve elements. In all three cases, the element size increases geometrically in the circumferential direction as indicated in Fig. 2(a). The model in Fig. 2(a) has ten elements in the circumferential direction. In the following comparison, the cladding region is represented by one layer of elements and the aspect ratio of the modified elliptical flaw geometry is 1:1.

In Fig. B.1, results are indicated at 25 min into the transient. It is observed that the calculated K_I values are very sensitive to mesh refinement in the circumferential direction. The K_I values increase by $\leq 60\%$ as the model is refined from six to ten elements. The K_I values further increase by $\leq 6\%$ as the model is refined from ten to twelve elements. Based on the evident slower rate of change in the K_I values as the model is refined from ten to twelve elements, it is believed that the vessel can be adequately modeled with a minimum of ten elements in the circumferential direction.

Figure B.1 Effects of mesh refinement in the circumferential direction of the vessel on the computed K_I values

Refinement in Cladding Region

In this study, all flaw geometries employ a common flaw depth of 6.35 mm (0.25 in.). The cladding region has a thickness of 2.769 mm (0.109 in.). Since the cladding thickness is significant relative to the flaw depth, an accurate calculation of the K_I values along the crack front requires that the stress distribution within the cladding region be accurately determined. The effects of mesh refinement for modeling the behavior of the cladding on the calculated K_I values is examined with models in which the cladding is represented with alternately a single layer or two or four layers of elements. In the following comparisons, the aspect ratio of the modified elliptical flaw geometry is 1:1.

In Fig. B.2, results are indicated at 25 min into the transient for models with one or two elements in the cladding region and ten elements in the circumferential direction of the vessel. No significant effect on the calculated K_I values along the crack front is observed. In Fig. B.3, results are indicated at 25 min into the transient for models with one or four elements in the cladding region and six elements in the circumferential direction of the vessel. Again, no significant effect on the calculated K_I values along the crack front is observed. The refinement insensitivity observed in Figs. B.2 and B.3 is a direct consequence of the observation that the distribution of (normal) tractions within the cladding region is essentially linear with wall depth.

Figure B.2 Effects of one or two layers of elements in the cladding region on the computed K_I values

Figure B.3 Effects of one or four layers of elements in the cladding region on the computed K_I values

As discussed in Sect. 4 of this paper, the calculated K_I values within the cladding region are very sensitive to the shape of the flaw within the cladding region near the inner-surface of the vessel. Consequently, it is emphasized that the observed refinement insensitivity is strictly applicable only to the modified-elliptical flaw geometry examined. Additional calculations are needed to evaluate the generality of this observation to other flaw geometries.

Influence of Finite Vessel Axial Length

Analysis results presented in the main body of this study are based on a vessel axial length of 152 cm (60 in.), which is representative of the extent of the belt-line region in a PWR-RPV. The total length of the RPV is, of course, much greater. In the case of flaws with an aspect ratio of 1:1, it is expected that the values would not change if the axial length is >152 cm (60 in.). This may not be the case for flaws with an aspect ratio of 80:1. Consequently, a limited number of analyses involving the modified ellipse and the canoe flaw geometry have been carried out to examine the influence of vessel axial length on the calculated K_I values along the crack front in the case of a flaw geometry with an aspect ratio of 80:1. These additional analyses are based on a vessel axial length of 304 cm (120 in.), and the finite-element models are constructed by suitably "stretching" the ends of the 60-in.-long finite-element models. No additional finite-elements are incorporated. In all cases examined, minimal influence of vessel axial length on the calculated K_I values along the crack front is observed.

Internal Distribution

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