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Heavy-Section Steel Technology Program Overview

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Abstract

This paper presents a status review of ongoing HSST program tasks aimed at refining the technology used in analysis of reactor pressure vessel fracture margins under pressurized thermal-shock (PTS) loading. Specific fracture-technology issues addressed include vessel flaw density and distribution, shallow flaws, fracture-toughness data transfer, circumferential cracks, ductile tearing and the influence of low-tearing toughness in stainless steel cladding. Preliminary results from the analysis and test programs are presented, together with interim assessments of their potential impact on a reactor vessel PTS analysis.

1. Introduction

Technology required for the accurate assessment of fracture-prevention margins in commercial nuclear reactor pressure vessels has been under continuous development for the past twenty-five years. The Nuclear Regulatory Commission (NRC)-funded Heavy-Section Steel Technology (HSST) Program was created to act as a focus for this essential safety-related development activity. The HSST Program, which is managed by Oak Ridge National Laboratory (ORNL), integrates input from a broad spectrum of national and international fracture mechanics research organizations to achieve the program objectives.

Early emphasis in the HSST Program was on development of fracture-mechanics methodology, and the associated materials fracture-toughness data base, for nuclear reactor pressure vessels. Results from this phase of the program were used to support the generation of regulatory instruments and national consensus Codes and Standards. This was followed by a phase of fracture technology validation using large-scale fracture mechanics tests with test articles fabricated from prototypical heavy-section reactor pressure vessel (RPV) steels. Results from these tests, and similar tests conducted by overseas fracture mechanics research organizations, identified a number of RPV fracture-technology issues.

In a parallel development, the growing body of surveillance program data from commercial nuclear power plants provided evidence that some reactor pressure vessels were accumulating irradiation damage at a rate higher than previously anticipated (Refs. 1 & 2).

Data from materials-irradiation research reactors provided identification of additional previously unanticipated irradiation damage trends (Refs. 3,4 and 5). The net effect of these developments

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was to require that reactor pressure vessel fracture-margin-safety assessments be performed in the near-term for a number of commercial nuclear power plants.

Pressurized-thermal-shock (PTS) has emerged as a loading condition of primary concern for reactor pressure vessel fracture-margin-safety assessments. Fracture technology issues with special significance to PTS analysis are of particular interest at this time. The focus of this paper is on ongoing HSST Program research and development (R&D) activities which relate directly to PTS analysis fracture issues. PTS R&D is an important element of the HSST program which is structured to address a broad spectrum of current fracture mechanics related licensing issues.

2 Background

A review of results from prior PTS analysis is useful in that it provides a basis for assessing the sensitivity of the analysis results to specific input parameters. The U.S. Nuclear Regulatory Guide for PTS analyses (Ref. 6) identifies the analysis of the H. B. Robinson nuclear power plant (Ref. 7) as a suitable generic model for PTS analyses. Results from that analysis therefore provide a suitable basis for the PTS-analysis input-parameter-sensitivity review.

PTS analysis of the reactor vessel for the H. B. Robinson nuclear plant did not predict sufficient through-wall fractures to provide an adequate demonstration of the PTS-analysis technology. To overcome this problem, the analysis of Ref. 7 also considered a hypothetical reactor vessel with weld-metal copper and nickel contents increased so as to produce accelerated irradiation embrittlement. The analysis also considered a hypothetical distribution of flaws in the reactor vessel wall. This vessel was designated the HBR-HYPO vessel. Results considered in this review of analysis experience are all for the HBR-HYPO reactor vessel.

The acceptance limit for a PTS-safety analysis is set in Ref. 6 in terms of a reactor vessel through-wall crack-penetration mean frequency which may not exceed 5×10^{-6} per reactor year. Analysis of the plant response to abnormal configurations and operating conditions leads to the identification of a broad range of transients which must be evaluated in the probabilistic PTS analysis. A small subset of these transients however account for a majority of the predictions of through-wall cracking. Transients in this category are termed the dominant transients. In the case of the HBR-HYPO vessel PTS analysis, 6 dominant transients accounted for 75% of all of the predictions of through-wall cracking. These transients, are listed in Table 1. Results from the analysis of the vessel response to these dominant transients provide the basis for this review.

Dominant transients listed in Table 1 are all the result of postulated failure of secondary side valves following a reactor trip. Valves involved are the steam power-operated-relief-valves (STM PORVs) and the steam dump-valves (SDV). These valves permit removal of heat from the reactor system in the event of a sudden loss of turbine load by dumping steam to the atmosphere (STM PORVs) or to the condensers (SDV). In the stuck-open condition, blowdown of the affected steam generators occurs, with an associated rapid removal of heat from the reactor primary coolant system.

Characteristics of two of the dominant transients for the HBR-HYPO reactor vessel are illustrated in Fig 1. These transients were selected because they bound the range of pressure-temperature time-history behavior for the transients in the dominant-transient set. Transient 9.43 represents a condition of maximum pressure loading, whereas transient 9.20B includes the most severe thermal-hydraulic boundary conditions. In the context of this review the point to note is that the transients span a range of pressure-temperature time-histories sufficiently broad to permit identification of any significant transient-dependant trends in the results.

Table 1. Six dominant transients account for 75% of the PTS Risk in the HBR-HYPO Vessel Analysis. The transients are all the result of a stuck-open secondary-side valve following a reactor trip.

TRANSIENT SEQUENCE IDENTIFICATION	THROUGH-WALL CRACK FREQUENCY (CRACK/YEAR)
9.41	4×10^{-9}
9.33	3×10^{-9}
9.19B	7×10^{-10}
9.94	7×10^{-10}
9.43	5×10^{-10}
9.20B	4×10^{-10}

Fig. 1. Transients considered in the PTS analysis of the HBR-HYPO Vessel covered a broad range of downcomer pressure-temperature time-history combinations.

A majority of the crack initiations predicted in the HBR-HYPO vessel analysis occurred for flaws in the first 2.5 cm (1 in.) of the vessel wall. A flaw depth of 1.19 cm (0.47 in.) ($a/w = 0.05$) was selected for this review as representative of conditions within the critical section of the wall. Crack-tip stress-intensity factor (K_I) time-histories for the selected surface flaw are given in Fig. 2.

Fig. 2. With the material RTNDT set at a maximum ($+2\sigma$) value of 270°F, the overlap of the $-2\sigma K_{IC}$ curve with the applied K_I curve remains in a region where $\dot{K}_I$ is negative. The warm-prestressing effects associated with these conditions were conservatively neglected in the initial analysis of the HBR-HYPO vessel.

for each of the bounding PTS transients. The curves of Fig 2 show the crack-tip stress-intensity factor to peak at about 20 minutes into the transients. Thereafter K_I decreases steadily in the case of Transient 9.20B. This is due to the decrease in thermal stress, as the vessel wall-temperature gradients collapse, exceeding the increase in stress generated by the increasing vessel internal pressure. In the case of transient 9.43 the rates of change of pressure and thermal stress are approximately equal and opposite for a 35-minute period, after which the net stress-intensity factor decreases steadily with time.

Superimposed on the crack tip K_I time-histories in Fig. 2 are the mean and -2σ critical-crack stress-intensity-factor (K_{IC}) curves. These curves were generated assuming a value of 270°F for RTNDT and therefore represent an extreme condition of K_{IC} overlap of the applied K_I curve. Even so, the area of overlap is confined to the portion of the K_I curve where K_I is changing very gradually with time. Under these conditions, the stress distribution in the vessel wall would approach a smooth curve with a minor perturbation adjacent to the inner surface due to the higher thermal expansion coefficient for the stainless steel cladding. The inference drawn from the data of Fig. 2 is that there is nothing in the characteristics of the applied-transient loading which would cause the distribution of crack initiations through the vessel wall thickness to depart markedly from a smooth curve.

It is evident from the curves of Fig. 2 that the overlap of the K_{IC} curves with the applied K_I curves occurs in a region where the slope of the K_I curve is negative. It has been shown that crack initiation will not occur when $\dot{K}_I < 0$ (the warm-prestressing effect). This condition is recognized in the H. B. Robinson PTS-evaluation report, and is evaluated in a sensitivity analysis performed after first completing the PTS analysis, neglecting the effects of warm prestressing. The net effect of the warm-prestressing sensitivity analysis was to reduce the predicted frequency of through-wall-cracking for the dominant transients by factors which vary from a low of 1/5 to a high of 1/300.

The distribution of fast-neutron fluence through the vessel wall and the resulting local shift in RTNDT is governed by the following equation (Ref. 7 and 8)

$$F = F_0 e^{-0.24a} \quad (1)$$

where

F = fast fluence at depth a in. (n/cm^2 : $E. > 1$ MeV)

a = crack depth (in.)

F_0 = fast fluence at the vessel inner surface (n/cm^2 : $E. > 1$ MeV)

$$\Delta RTNDT = [-10 + 470 \text{ Cu} + 350 \text{ Cu Ni}] (F \times 10^{-19})^{0.27} \quad (2)$$

Where

Cu, Ni = concentrations of copper and nickel, wt%.

Combining equations (1) and (2) the variation of RTNDT through the vessel wall is seen to be proportional to $e^{-0.0648a}$. The values of RTNDT at depths of 2.54 cm and 7.62 cm (1 and 3 in.) beneath the vessel inner surface would thus be 94% and 82% of the inner-surface value,

respectively. This gradual variation of RTNDT through the thickness of the vessel wall would be expected to influence the distribution of crack initiations in a like manner.

The cumulative distribution of crack initiations for each of the dominant transients in Table 1 is shown plotted as a function of the depth of the surface crack in Fig. 3. The curves of Fig. 3(a)

Fig. 3. Cumulative crack-initiation curves from an OCA-P analysis of five different transients are remarkably similar. This suggests the shape of the cumulative crack-initiation curve is dictated by the assumed flaw distribution with analysis input parameters for specific transients having a second-order effect.

show that the majority of all cracks that initiate originate from flaws located in the inner 2.5 cm (1 in.) of the vessel wall. The normalized, cumulative crack-initiation curves of Fig. 3(b) show furthermore that the cumulative crack-initiation curves for each of the six dominant transients are remarkably similar.

The remaining parameter which can influence the through-thickness distribution of crack initiations is the input flaw distribution. The similarity of the normalized, cumulative crack-initiation curves for widely differing dominant transients points to the input flaw distribution as the dominant influence on the distribution of crack initiations.

Conclusions drawn from this brief review of the HBR-HYPO reactor vessel PTS analysis are:

- (a) The major portion of the predicted crack initiations originate at flaws located in the inner 2.5 cm (1 in.) of the reactor vessel wall.
- (b) Factors which influence the fracture toughness of material within the inner 2.5 cm (1 in.) of the vessel wall can have a significant impact on the outcome of a reactor vessel probabilistic PTS analysis. Factors in this category include cladding effects, restraint release due to the proximity of a free surface (shallow flaws), metallurgical gradients and possibly conditioning of the material by subcritical cyclic straining.
- (c) The input flaw density and distribution functions have a dominant influence on the number and distribution of predicted crack initiations.

3. Flaw Density and Distribution

The reactor vessel flaw population is at the same time both the most influential and the most ill defined of the input parameters to a PTS analysis. The problem stems from an absence of relevant data. Nondestructive volumetric-examination techniques in use at the time most of the reactor vessels currently in service were constructed lacked the detection and resolution capability to accurately characterize the population of small, sharp-edged flaws. These are the flaws of primary concern in a PTS analysis. The problem is compounded by the fact that the flaws of primary concern are those located close to the inner surface of the reactor vessel. In this region the detection and sizing capability of NDE systems is further degraded by the proximity of the stainless steel reactor vessel cladding.

Flaw density and distribution estimates used in the PTS analysis of Ref. 7 were taken from a study performed by the United Kingdom Atomic Energy Authority under the chairmanship of Dr. W. Marshall (Refs. 9 & 10). The Marshall report flaw data were taken from ultrasonic inspections of 44 LWR vessels made "essentially to ASME III rules". Ultrasonic inspections were made at approximately the time the vessels entered service. The flaw population discovered in these inspections can therefore be considered relevant to the population of LWR vessels as delivered to nuclear plants.

Twelve defects were found in the inspection of 44 vessels. The defects were all in the size range 1.25 cm (0.5 in.) $< a < 2.5$ cm (1.0 in.). No defects were found with a size in excess of 2.5 cm (1.0 in.). Data from a non-nuclear vessel (Ref. 11), which failed during its hydro test due to the presence of a large flaw, were used to supplement the nuclear vessel data base.

The absence of data on defects in the size range $a < 1.25$ cm (0.5 in.) was not considered to be a concern in the evaluations of Ref. 9. The basis for this position was an assessment that defects in the size range $a < 1.25$ cm (0.5 in.) would not grow to a significant size provided the vessel material toughness remained on the upper shelf. Results from the HBR-HYPO vessel PTS analysis however indicate that a major portion of the crack initiations occurred at defects in this size range. It is important to recognize therefore that the significance of shallow flaws incorporated into the Marshall report assessment of flaw densities differs from that inferred from the HBR-HYPO PTS analysis. It is also important to recognize that the absence of data on flaws in the size range $a < 1.25$ cm (0.5 in.) is probably a reflection of the detection and sizing capabilities of the ultrasonic equipment and procedures used in the inspection rather than an indication of the absence of flaws in this size range.

Inspection results were used to derive functions which define the probability of having a flaw in the reactor vessel welds in a size (through-thickness dimension) range Δa . The probability was expressed in terms of a flaw density, a flaw-size distribution function and a detection-probability function. The latter function represents the probability of detecting the flaw during a preservice-ultrasonic inspection. As used in the PTS analysis of Ref. 7, the flaw density, distribution and detection-probability functions have the following form.

$$P(\Delta a_i) = NV \int_{\Delta a_i} f(a)B(a)da \quad (3)$$

$$f(a) = 4.1e^{-4.1a} \quad (4)$$

$$B(a) = 0.005 + 0.995e^{-2.87a} \quad (5)$$

Where

$P(\Delta a_i)$ = The probability of having a flaw in a specific region within the depth range Δa_i

$f(a)$ = flaw-depth-density function

$B(a)$ = probability of nondetection in a preservice or in-service ultrasonic inspection.

N = number of flaws of all depths per unit volume

V = volume of the specific region

a = crack depth, in., (the analysis of Ref. 7 used English units)

and

$$\int_0^{\infty} f(a)da = 1.$$

Generation of the flaw-population definition embodied in Eqs. 3, 4 and 5 represents a considerable achievement. Consideration of modified-flaw population representations is possible at this time because of (a) insights obtained from the PTS analysis of the HBR-HYPO reactor vessel into the effect of flaw density and distribution assumptions on the analysis results and (b) additional flaw density and distribution data obtained from the non destructive and destructive examination of welds cut from surplus reactor vessels.

Equations 3, 4 and 5 provide a definition of the number and size distribution of flaws located within a given volume of weld metal, but they provide no information on the spatial distribution of the flaws within the thickness of the vessel wall. The data base from which the flaw population density and distribution functions were generated was comprised of flaws discovered using ultrasonic inspection. Given the ultrasonic inspection technology and practices in use at the time these data were collected it is reasonable to assume that the flaws in the data base were located in the body of the weld. It was common practice at that time to exclude the surface volume of the weld from the ultrasonic inspection gate to avoid the problems of spurious signals caused by the inner surface stainless steel cladding. It is considered unlikely therefore that the flaws included in the data base were located in the region of the weld adjacent to the inner surface of the reactor vessel.

The analysis of Ref. 7 considered all flaws to be surface flaws. This approach assured a conservative result from the analysis since, given identical boundary conditions, K_I for a surface flaw is greater than that for a buried flaw of the same radial depth by a factor of $\sqrt{2}$. The surface flaw assumption had the additional effect of imposing a spatial distribution on the flaws which did not derive from the data base.

The imposed spatial distribution of flaws deriving from the surface-flaw assumption placed the major portion of the flaw population in a region of the vessel wall where both irradiation-embrittlement effects and transient thermal stresses are at a maximum. Figure 3(b), which shows the normalized, cumulative crack-initiation curves to be very similar for a broad range of PTS transients, suggests that the imposed spatial distribution of the flaw population has a dominant effect on the analysis results.

Close coupling between the input spatial flaw distribution and the output crack initiation distribution is further illustrated in Fig. 4(a). The mean cumulative crack initiation curve for the five dominant transients is seen to have characteristics similar to those of the cumulative flaw-distribution curve. Application of a relatively simple correlation function to the input cumulative flaw distribution can produce a close fit to the normalized cumulative crack-initiation curves for a broad range of PTS transients. An estimated normalized, cumulative crack-initiation curve produced in this manner is shown in Fig. 4(b). It is evident from Fig. 4(b) that the assumed spatial distribution of the input flaw population has a dominant influence on the normalized cumulative crack-initiation output curve from a PTS analysis.

Thermal-stresses and irradiation-damage levels are highest in material adjacent to the inner surface of the vessel. By placing the majority of the flaws in this region, the assumed spatial distribution of vessel flaws influences both the distribution and frequency of crack initiations. The influence on crack-initiation frequency is in addition to that associated with the conservative crack depth used in calculation of crack-tip stress-intensity factors. Interest in the influence of the assumed spatial distribution of vessel flaws derives primarily from its effect on crack-initiation frequency.

At this point it would be possible to conclude that the imposition of a spatial distribution on the flaw population which did not derive from the flaw data base had introduced an element of

Fig. 4 The normalized average cumulative crack-initiation curve for five PTS transients is similar to the cumulative flaw-distribution curve. An estimated cumulative crack-initiation curve for the NUREG/CR-4183 analysis can be generated by factoring the input-flaw distribution by a linear-hyperbolic correlation parameter.

conservatism into the analysis. A review of the impact of subsequent additions to the flaw data base however leads to an opposite conclusion.

Weld material cut from the surplus Hope Creek BWR vessel was recently inspected by ORNL (Ref. 12). One flaw was found in the inspection of 0.03 m³ of weld metal. Non-destructive

examination of material from the surplus Midland PWR vessel is currently in progress at the EPRI NDE Center at Charlotte, NC (Ref. 13). To date this examination has revealed two to three subclad indications in an examination of 8.2 m (27 ft.) of weld material. Note that all of the indications/flaws found are located within 2.5 cm (1 in.) of the vessel inner-wall surface.

It is evident from the prior discussion that the flaw population exerting the dominant influence on crack initiation is that located within 2.5 cm (1 in) of the vessel wall inner surface. The vessel weld examinations described above have provided data on both the density and spatial distribution of flaws. To evaluate the significance of these data, it is therefore convenient to express all of the flaw densities in terms of that volume of weld metal located within 2.5 cm (1 in) of the vessel inner surface.

The flaw density used in the PTS analysis of Ref. 7 was 1 flaw/m³. Integration of the flaw- depth density function of Equation 4 between the limits of a = 0 to 1 in (2.5 cm) gives a value in excess of 98% for the subset of the total flaw population located in this region of the vessel wall. The wall thickness of the HBR-HYPO reactor vessel was 23.88 cm (9.4 in). In terms of the flaw density in the critical inner 2.5 cm (1 in.) of the vessel wall therefore the flaw density used in the analysis translates into a "surface density" N_s of

$$N_s = 1 \times 0.98 \times 23.88/2.5 = 9 \text{ flaws/m}^3$$

The Hope Creek vessel plate was 17.1 cm (6.73 in) thick. The surface-flaw density for this material is therefore

$$N_s = (1/0.03) \times (17.1/2.5) = 228 \text{ flaws/m}^3$$

In the case of the Midland reactor vessel material it is important to recognize that characterization of the indications has not yet been completed. If however the 3 subclad indications are confirmed as flaws and the weld width within the surface volume is taken as 5 cm (2 in), the surface-flaw density is obtained as

$$N_s = 3 / (0.025 \times 0.05 \times 8.2) = 293 \text{ flaws/m}^3$$

The recent reactor vessel weld flaw-population data, while admittedly sparse, indicate that the flaw density used in the PTS analysis of Ref. 7 may have been low by a factor in excess of one order of magnitude.

The final factor to be considered in the evaluation of the input flaw density and distribution functions is the "nondetection probability" factor B(a), as defined in Equation 5. The probability of detection-factor curve [1-B(a)] is shown in Fig. 5 superimposed on envelope plots of (a) the detection capability of ultrasonic testing technology as determined in the PISC II round-robin UT trials (Ref. 14) and (b) the crack-initiation profiles for the dominant transients from the PTS analysis of Ref. 7.

It is evident from the plots of Fig 5 that the flaw-detection capability estimates used to derive Equation 5 were optimistic. Equation 5 predicts a flaw detection rate in excess of that achieved by the highest performing team in the PISC trials for a major portion of the flaw- size range of interest in a PTS analysis. It should be noted that the PISC II results show better detection performance for volumetric flaws. The results of interest relative to the PTS analysis are those for the sharp-edged cracks however, and the detection rates for those cracks were as indicated in Fig. 5.

Fig. 5 PISC II NDE results obtained using ASME procedures with 20% DAC indicate a low probability of detecting the smooth sharp-edged cracks of concern in a PTS analysis.

The PISC II results show that inspection teams performing at the lower bound of the performance envelope would have a very low detection rate for the flaws of interest in a PTS analysis. These results indicate it would be prudent to consider elimination of the flaw-detection element from the PTS analysis until such time as the performance of ultrasonic NDT systems is substantially improved.

4. Shallow Flaws

Shallow-flaw fracture-toughness tests have been performed at the University of Kansas (U of Kan.) under the direction of Dr. S. T. Rolfe (Refs. 15 and 16). Materials investigated in the U. of Kan. program to date include A36 ($\sigma_y = 36$ ksi) and A517 ($\sigma_y = 100$ ksi). Yield strengths for these materials bracket that of A533B ($\sigma_y = 68$ ksi), used in the construction of many PWR and BWR pressure vessels. It is anticipated therefore that A533B will exhibit the enhanced shallow-flaw fracture toughness demonstrated in the U. of Kansas A36 and A517 programs.

Shallow-flaw fracture-toughness curves from the U. of Kansas A36 and A517 programs are reproduced in Figs. 6(a) and 6(b), respectively. The shallow-flaw fracture-toughness-elevation effect is seen to increase from zero on the lower shelf to a maximum in the lower-transition region of the curves. PTS analysis shows that crack initiations within the inner 2.5 cm (1.0 in.) of the reactor vessel wall typically occur at crack-tip temperatures corresponding with lower transition-zone temperatures for irradiated reactor vessel material. It is to be anticipated therefore that elevated shallow-flaw fracture-toughness properties could have a significant influence on results from a PTS analysis.

Fig. 6. Shallow-flaw fracture toughness tests performed at the University of Kansas have shown elevated fracture toughness in A36 ($\sigma_y = 36$ ksi) and A517 ($\sigma_y = 100$ ksi). Similar shallow-flaw behavior is anticipated in A533B ($\sigma_y = 68$ ksi).

A sensitivity analysis was performed to determine the potential impact of an A533B shallow-flaw fracture-toughness data base on the outcome of a reactor vessel PTS analysis. This analysis was performed as a part of the screening process to determine the utility of adding a shallow-flaw fracture-toughness-testing task to the HSST Program.

Shallow-flaw fracture-toughness curves for A533B were estimated by interpolating between the shallow-flaw curves for A36 and A517 given in Fig 6. The procedure used first estimated A533B curves for a/w ratios of 0.15 and 0.5 using the material yield stress as the interpolation parameter. In the case of the A36 test specimens, a/w ratios of 0.15 and 0.5 corresponded to absolute flaw depths of 4.8 mm (0.19 in.) and 16 mm (0.63 in.), respectively.

The relative influence of the flaw-depth ratio (a/w) and the absolute flaw depth (a) on shallow-flaw behavior remains an open issue at this time. In the following evaluation the absolute flaw depth (a) was used as the controlling parameter. This approach should produce a conservative estimate of the potential benefits of enhanced shallow-flaw fracture toughness since the effects were assumed not to exist beyond the maximum flaw depth of 16 mm tested in the U. of Kan. program.

Shallow flaw fracture-toughness enhancement was assumed to vary in a manner producing the maximum enhancement at a flaw depth of 4.8 mm (0.19 in.) and zero enhancement at a flaw depth of 16 mm (0.63 in.). Variation of shallow flaw fracture toughness enhancement between these two bounding crack-depth values was assumed to be controlled by a modified-sine curve. Results from this estimation procedure were expressed in terms of factors giving the increase in fracture toughness for a given crack depth relative to an assumed lower-bound (-2σ) value at a crack depth of 16 mm (0.63 in.). These factors were then applied to the ASME Section XI K_{IC} curve to produce estimated -2σ shallow flaw K_{IC} curves for use in an exploratory PTS analysis.

Analysis of the HBR-HYPO vessel response to the 9.22B PTS transient was used to evaluate the effect of enhanced fracture toughness for shallow flaws. Crack-initiation data from the original analysis reported in Ref. 7 are shown in Fig. 7 (a), together with the associated critical crack-depth curves in Fig. 7(b). The area under the crack-initiation curve for the original analysis of Ref. 7 provides the baseline for assessing the potential effect of shallow-flaw fracture-toughness enhancement.

Results from an OCA-P analysis of the response of the HBR-HYPO vessel with enhanced shallow-flaw fracture toughness are shown superimposed on the critical crack-depth curves of Fig. 7(b). The intercept of this curve with the warm-prestressing line defines the minimum depth for crack initiation with enhanced shallow-flaw fracture-toughness properties. Projection of this intercept to the crack-initiation plot shows that approximately 2/3 of the previously predicted crack initiations would be eliminated by the enhanced shallow-flaw fracture toughness. Given the conservative nature of this scoping evaluation, the result obtained provides justification for an experimental investigation of shallow-flaw fracture-toughness enhancement in reactor pressure vessel steels.

Preparations for shallow-flaw fracture-toughness testing of A533B specimens within the HSST program are well advanced. The specimen selected for the initial exploratory tests is a 5.08 cm (2.0 in.) wide and 10.16 cm (4.0 in.) deep single-edge notched beam. The beam has a span to depth ratio of 4 and is loaded in 3-point bending. Crack depth (a) to beam depth (w) ratios vary from a minimum of 0.05 to a maximum of 0.5. Figure 8 shows some of the development batch of test specimens, while Fig. 9 shows a beam specimen loaded into the 3-point bend test fixture in an Instron model 1336 servo-hydraulic test machine.

Fig. 7. Inclusion of an estimated shallow-flaw fracture-toughness increase in a PTS analysis using the H. B. Robinson 9.22B transient results in a significant decrease in the number of predicted crack initiations.

Fig. 8. Test specimens used in the A533B shallow-flaw fracture-toughness testing program are 5.08 cm (2 in.) wide and 10.16 cm (4 in.) deep. Flaw a/w ratios range from 0.05 min. to 0.5 max.

Fig. 9. The shallow-flaw fracture toughness test specimens are loaded in 3-point bending in an Instron model 1336 Servo hydraulic test machine for both fatigue precracking and fracture-toughness testing.

Elastic-plastic finite-element analysis of the test specimens was performed as a part of the specimen-design process. Results from these analyses are of interest in that they illustrate the growth of the crack-tip yield zone believed responsible for the relaxation of crack-tip constraint in shallow flaws. These results are shown in Fig. 10 in the form of elastic and plastic zones in the specimen at loads estimated to generate crack-tip K_I values equal to K_C at the planned test temperature of -60°C (-76°F). Values of K_C used in this analysis were estimated using a procedure similar to that described previously. In this case, however, the baseline used to estimate the enhanced shallow flaw fracture toughness was the mean fracture toughness derived from the ASME Section XI K_{IC} -data base. RT_{NDT} for the test material -35°C (-31°F) was obtained from characterization tests (Ref. 17).

The results of Fig. 10 show clearly the loss of plastic-zone constraint at the onset of fracture for crack depths in the range 0.51 cm (0.2 in.) $\leq a \leq 1.02\text{ cm}$ (0.4 in.). The cumulative crack-initiation curves of Fig. 3B show this to be a crack-depth range in which a substantial portion of the predicted crack initiations occur. These analysis results reinforce the assessment that enhanced shallow-flaw fracture toughness may have a significant impact on the PTS analysis of reactor pressure vessels.

Fig. 10. Elastic-plastic analysis of the 10.16 cm (4 in.) deep beam specimens for the shallow-flaw testing program clearly shows loss-of-constraint at the crack tip for a/w ratios less than 0.10. The enhanced fracture toughness observed for shallow flaws is attributed to uncontained crack-tip yielding.

5.0 Fracture Technology Issues

A number of generic fracture technology issues have been identified in recent years. The HSST program is currently addressing three such issues which have the potential for significant impact on the analysis of PTS events. The issues of concern here are (a) fracture toughness data transfer, (b) circumferential cracks, and (c) ductile tearing.

5.1 Fracture Toughness Data Transfer

Results from the analysis of the HSST program wide-plate crack-arrest specimens provide a convenient illustration of the fracture-toughness data transfer issue (Ref. 18). Materials characterization data, obtained from tests using 1T, 2T, and 4T compact-test specimens were used to generate pretest predictions of the wide-plate specimen crack-initiation loads. Loads required to initiate fracture in the wide-plate tests were substantially higher than had been predicted. K_I/K_{IC} ratios at crack initiation for the Series 1 wide-plate tests were in the range $1.96 \leq K_I/K_{IC} \leq 3.97$.

The discrepancy between the predicted and observed crack initiation toughnesses was in a conservative direction in the case of the wide-plate Series 1 test specimens. There is no basis, however, for concluding that this would always be the case. The discrepancy in the predicted fracture behavior of large-scale structures fabricated from well-characterized material raises a basic issue relative to the adequacy of the linear elastic fracture mechanics technology used to generate the pretest fracture predictions. In this connection, it is appropriate to note that a substantial portion of the K_I/K_{IC} discrepancy remained after corrections had been made for the potential loss-of-plane strain constraint at the crack tip using Irwin's β factor.

An ongoing investigation within the HSST program seeks to obtain an improved understanding of relationships governing the transfer of fracture toughness data from small-scale specimens to large-scale applications (Ref. 19). Two basic approaches are being investigated. The first approach is aimed at developing a micro-mechanical model in which fracture is governed by fundamental material properties. Objectives of this approach are to generate a fracture model and supporting materials data which can be applied under conditions when single parameter characterization of the fracture process is no longer valid. This approach is in the early stages of development.

The second approach seeks to develop auxiliary fracture characterization parameters which can be used, in conjunction with existing correlation based fracture parameters, to improve the accuracy of fracture predictions. Preliminary results from the approach have indicated that auxiliary fracture correlation parameters exist which may be capable of development to meet the task objectives. The parameter showing most promise at this time is one based on the area (A_{CR}) bounded by a crack tip stress contour corresponding to Hahn's (Ref. 20) estimate of the cleavage-crack propagation stress for ferrite grains.

Fine-mesh, elastic-plastic finite-element analysis of the wide-plate Series 1 test specimens and the associated compact test characterization specimens is the technique used to search for auxiliary fracture-correlation parameters. Data for the specimens (structures) considered in this study are given in Fig. 11. The specimens can be seen to differ considerably in both geometry and size.

Finite-element models used in the study are illustrated in Fig. 12. Elastic-plastic analyses of these 2-D and 3-D models were performed using the ADINA computer program (Ref. 21). Results obtained from the analyses are shown in Fig. 13 in the form of the calculated crack tip J

Fig. 11. Analysis models used in this study represent fracture-toughness test specimens differing widely in both geometry and size.

Fig. 12. Detailed 2-D and 3-D finite-element models of the wide-plate and compact-test specimens were used in an evaluation of potential supplementary fracture toughness parameters.

Fig. 13. Initial results show an improved transferability of fracture-toughness data using an additional parameter based on the crack-tip area enclosed within a stress contour corresponding with Hahn's estimate of the cleavage crack-propagation stress for ferrite grains (1380 MPa).

integral plotted as a function of the area within the critical crack-tip stress contour. Each of the four wide-plate specimens analyzed is seen to produce a unique J- A_{CR} curve. The upper curves are for WP-1.8, which was performed on a 15.24 cm (6 in.) plate with a chevron crack front configuration at -47°C and WP-1.2, in which a 10.16-cm-plate (4 in.) without chevrons was tested at -33°C . RTNDT for the A533B material used in these tests was -23°C (-73°F). Fracture loads for the WP-1.8 and 1.2 specimens were 26.5 MN (5.96×10^6 lb) and 18.9 MN (4.25×10^6 lb), respectively. The lower wide-plate specimen curves in Fig. 13 are for specimens 1.3 and 1.6, both of which were 10.16 cm (4 in.) wide specimens with a chevron crack-front configuration. Symbols on the curves denote the point of fracture in each test.

The lower two curves in Fig. 13 are from the analyses of the compact-test specimens used to characterize material properties for the A533B wide-plate specimens. Again, the symbols on these curves denote specimen failure points. Verticals erected through the midpoint of the characterization results for test temperatures of -18°C and -75°C define a range of areas (A_{CR}) within the critical crack-tip stress contour. The significance of this area (A_{CR}) as a potential auxiliary fracture-toughness correlation parameter derives from the fact that 3 of the 4 wide-plate specimens, which were tested at temperatures in the range $-75^{\circ}\text{C} < T < -18^{\circ}\text{C}$, failed when the calculated crack tip A_{CR} values were within the range of A_{CR} values derived from small specimen tests conducted at -75°C and -18°C . The remaining wide-plate specimen (WP-1.3) failed at an A_{CR} value within the scatter band for the compact test specimen A_{CR} values at -75°C .

The area within the critical crack tip stress contour (A_{CR}) shows promise as an auxiliary fracture-toughness correlation parameter to improve the accuracy of fracture predictions involving specimen size related fracture toughness data transfer. It is evident from the scatter of results from the compact test specimens, however, that further work is required to develop these initial results into a useful auxiliary fracture parameter for use in pressure vessel analysis. Development of the A_{CR} auxiliary fracture toughness correlation parameter is continuing within the HSST Program.

5.2. Circumferential Cracks

A review of issues associated with the analysis of fracture margins for circumferential cracks in reactor pressure vessels was given in Ref. 22. One of the open issues identified was the effect of a positive strain parallel to the crack front on the crack initiation toughness K_{IC} . An investigation of this condition, which is illustrated in Fig. 14, is the subject of an ongoing element of the HSST Program.

Investigation of the effects of positive out-of-plane strain effects is proceeding using a fine-mesh 3-D elastic-plastic finite-element analysis of a compact test specimen slice to predict the effect on crack-tip stress and strain contours. Auxiliary crack-tip fracture-correlation parameters, including the A_{CR} parameter discussed previously, are then used to predict the effect of out-of-plane strains on crack-initiation toughness. Available plane-stress and plane-strain fracture-toughness data provide a means of validating the models used in the range $\epsilon_z \leq 0.0$.

Figure 15 shows the geometry of the finite-element model used in this study. The model represents a 2.0 mm (0.08 in.) thick slice from a 1T compact-test specimen. The region immediately surrounding the crack tip is modelled in a fine-mesh fan configuration to provide the required definition of crack tip stress and strain contours. Generalized plane-strain boundary conditions were used to impose out-of-plane strains (ϵ_z) on the model. Both loads and boundary displacements were applied incrementally up to their maximum values. The maximum load of 0.035 MN (7.9 kips) corresponds to the fracture load of one of the 1T-CT specimens used to

Fig. 14. Positive-hoop strains parallel to the crack front coupled with a higher hydrostatic/deviatoric stress ratio may reduce the material ductile tearing toughness for circumferential flaws below that associated with axial flaws.

Fig. 15. A thin three-dimensional model of a 1T-CT specimen is used to study out-of-plane strain effects.

characterize material properties for the Series 1 wide-plate tests. A second specimen failed at an applied load of 0.028 MN (6.3 kips). Both loading conditions were analyzed in this study. Transverse strains of $\pm 0.14\%$ and $\pm 0.114\%$ were applied for the 0.035 MN- and 0.028 MN-loading conditions, respectively.

Results from the ADINA analysis of the 0.035 MN CT-specimen slice model are reproduced from Ref. 19 in Fig. 16. Significant reductions in both the area of the plastic zone and the crack-tip plastic-zone radius are seen to occur as the out-of-plane strain (ϵ_z) transitions from -0.14% to $+0.14\%$. A reverse trend is observed in the case of the area A_{CR} within the critical crack-tip stress contour.

Plots of the crack-tip J as a function of A_{CR} are given in Fig. 17 for each of the out-of-plane generalized plane-strain conditions analyzed for the 0.028 MN CT specimen. These results indicate that a positive out-of-plane strain ($\epsilon_z = 0.114\%$) produces a reduction in crack-tip J at the A_{CR} value corresponding with J_{IC} in the $\epsilon_z = 0$ specimen. An opposite result is seen to exist for the case of the $\epsilon_z = -0.114\%$ analysis. These results indicate some reduction in fracture toughness under conditions similar to those existing in the circumferential weld of a reactor pressure vessel. A_{CR} appears to have the potential for development into an auxiliary correlation parameter which can be used to improve fracture predictions involving fracture-data transfer. Further work is required however to develop and validate this concept. In particular, the performance of the A_{CR} parameter needs to be tested further in the plane stress ($-\epsilon_z$) to plane-strain ($\epsilon_z = 0$) domain using available fracture-toughness test data to check the consistency of its predictions and establish performance limits. This work is planned as a continuing element of the HSST Program.

Fig. 16. The plastic-zone area decreases and area enclosed within a given principal stress contour increases with increasing out-of-plane strain.

Fig. 17. The applied J required for initiation is shown to be a function of the out-of-plane strain condition.

Examination of the plots of Fig. 16. shows that the zone of plastic straining surrounding the crack tip is also influenced significantly by the application of out-of-plane (ϵ_z) strains. The width of the plastic zone, along the crack-extension path (r_y), is seen to be particularly sensitive to the generalized plane-strain boundary condition (ϵ_z)

A relationship between the plastic zone size (r_y) at instability and the material fracture toughness is developed in Ref. 23. Application of this relationship provides an additional means of assessing the effect of a positive out-of-plane strain on fracture toughness. The relationship has the following form:

$$r_y = \frac{1}{\beta \cdot \pi} \left(\frac{K_I}{\sigma_y} \right)^2 \quad (6)$$

where β is a constraint factor having a value of 2.0 for plane stress and 6.0 for plane-strain conditions, and σ_y is the material-yield stress.

Application of Eq. (6) to the evaluation of the plastic zone radius (r_y) data of Fig. 16 requires development of β factors for conditions other than those described above. This development has not yet been completed. It is apparent however from the results shown in Fig. 16 that the ratio of crack tip r_y values for the case of $\epsilon_z = -0.14\%$ and $\epsilon_z = 0\%$ is close to 3.0. Under these conditions the value of K_I at instability for the $\epsilon_z = -0.14\%$ case would approach the plane stress fracture toughness K_{IC} , which is significantly greater than the plane strain fracture toughness K_{IC} associated with the $\epsilon_z = 0\%$ case. The factor of 3 increase in r_y between the $\epsilon_z = 0\%$ and $\epsilon_z = -0.14\%$ cases is therefore associated with a fracture toughness increase of the order of K_{IC}/K_{IC} . Application of similar reasoning to the $\epsilon_z = +0.14\%$ case, where the ratio r_y/r_{y0} is approximately 0.15, would indicate that the fracture toughness for the $\epsilon_z = 0.14\%$ case could be significantly lower than that for the plane strain ($\epsilon_z = 0\%$) case.

It is apparent from the prior discussion that of the two candidate auxiliary fracture-toughness-correlation parameters under evaluation (A_{CR} and r_y) currently indicate significantly differing impacts for a positive out-of-plane strain. Further work is required to resolve these apparently conflicting results. It is anticipated that large-scale fracture tests under conditions of biaxial loading will be required to refine and validate an auxiliary fracture toughness parameter. What limited test data exist on fracture under biaxial loading conditions (Refs. 24 and 25) tend to indicate a much weaker influence of out-of-plane strains on K_{IC} than would be inferred from the plastic-zone radius correlation.

The interim conclusion from this work is that the auxiliary fracture-toughness-correlation parameters currently under investigation have the potential to provide a guide to the adjustment of fracture toughness required (if any) for the analysis of circumferential cracks. Considerable further development is required however before the auxiliary fracture-toughness-correlation parameter can be considered a useful fracture-analysis tool.

5.3 Ductile Tearing

Ductile tearing in advance of cleavage fracture has been shown in both analysis (Ref. 26) and test (Ref. 27) to be a fracture mode of concern in the PTS analysis of reactor vessels containing low upper-shelf (LUS) weld material. This material exists in 17 vessels currently in service within the U.S. electric utility system (Ref. 28).

In the context of a PTS analysis the primary concerns relating to ductile tearing are (a) modification of the initial-flaw geometry, (b) a potential decrease in the cleavage-crack initiation toughness due to the strain-rate effects induced by ductile tearing and, (c) ductile tearing instability as the ultimate reactor vessel failure mode.

A comprehensive review of the state-of-the-art of ductile tearing technology was conducted in an International Workshop held May 8-10, 1990, in Boston, Mass. The workshop was organized by the OECD Committee on Safety of Nuclear Installations/Fracture Analysis Group (CSNI/FAG). An effective evaluation of the state-of-the-art of ductile tearing technology was achieved by means of comparing analyses, performed by the workshop attendees, of large-scale fracture experiments from a number of the participating countries.

Results from the CSNI/FAG Workshop are still under evaluation by the workshop participants. No consensus assessment of the state-of-the-art of ductile tearing technology is available from the

workshop at the time of writing. It appears likely, however, that some of the fracture experiments analyzed involved crack-tip behavior which may not be adequately characterized by a single (J integral) fracture-correlation parameter.

Heavy-Section Steel Technology-program efforts directed at resolution of the ductile tearing issue are centered on the development of a crack tip micro-mechanical model. The intent of this development, which is an extension of the model development described briefly in Section 5.1, is to produce a model which can predict tearing based upon fundamental material properties. Such a model should have the capability to predict tearing behavior in conditions where single-parameter characterization of ductile tearing behavior are no longer adequate.

Development and validation of ductile tearing technology improvements to resolve the issues currently identified appears to be unavoidably a long-range process. Interim assessments of the effects of ductile tearing may be required to meet near-term requirements for PTS evaluations. A recently completed HSST program study of the impact of ductile tearing on the utility of extended crack-arrest-toughness curves (Ref. 29) provides an example of such an interim assessment. The study of Ref. 29 showed that ductile tearing effectively negates most of the PTS analysis advantages deriving from an extended cleavage crack-arrest-toughness curve. Similar sensitivity evaluations of the effect on a PTS analysis of ductile tearing in advance of cleavage fracture are included in the near-term HSST Program plan.

6. Cladding

Cladding has typically been regarded as a tough inner skin to the reactor vessel with a capability to impart an additional increment of fracture margin by acting to reduce the crack-tip opening of submerged cracks. This condition is illustrated in Fig. 18(a). While thermal expansion and heat-transfer properties of the stainless steel cladding were included in the PTS analysis of Ref. 7, effects of the enhanced cleavage fracture toughness of cladding were conservatively neglected.

Interest in cladding derives from its potential influence on crack initiation in the vessel shell structural material. Results from fracture tests on clad plates (Ref. 30) have shown that a fast-running crack will tunnel under cladding. Cladding, therefore, appears to have only a minor influence on structural margins once cleavage cracking of the shell material has been initiated.

Data generated recently in the reactor materials irradiation program (Ref. 5) have raised a concern relative to a possible negative effect of cladding on pressure vessel fracture margins during a PTS event. The concern derives from the low ductile tearing toughness of irradiated cladding. Figure 19 shows the test results of concern. Tearing toughness of irradiated stainless steel cladding is seen to be similar to that of irradiated LUS-weld material, and substantially less than that of irradiated A533B vessel shell material. A concern exists therefore that tearing could initiate in the cladding and convert to cleavage fracture at the cladding-base material interface. Strain-rate effects induced by the tearing process add to the cleavage fracture-initiation concern.

The objective for the initial phase of this study is to determine the potential for clad ductile tearing initiation in advance of cleavage-fracture initiation in the base material for two flaw configurations of particular interest in a PTS analysis. These are (a) the semielliptical-surface flaw and (b) the two-dimensional subclad flaw. Conceptual features of these flaws are illustrated in Fig. 18. Initial evaluations have focused on the semielliptical-surface flaw.

Fig. 18. Effects of the vessel cladding are not yet fully included in the reactor vessel PTS analysis models (OCA-P). Additional research is required to determine if their introduction will have a net positive or negative effect on the calculated reactor vessel failure probability.

Fig. 19. Recently completed tests to measure the ductile tearing toughness of reactor vessel materials have shown ductile-tearing initiation toughness of both LUS weld material and cladding in the irradiated condition to be approximately half that of irradiated A533B material.

Figure 20 shows the 3D elastic-plastic finite-element model used in the evaluation of clad-tearing events in surface flaws. A 6:1 aspect ratio semielliptical surface flaw with a maximum depth of 1.78 cm (0.7 in.) was used in this evaluation. Generalized plane-strain boundary conditions were imposed on the vertical surfaces of the model in order to simulate a level of deformation restraint consistent with that encountered in a reactor pressure vessel shell. Mechanical and physical properties for Type 304 stainless steel and A533B low-alloy steel were used for the cladding and base material, respectively.

Transient pressure and temperature loading used in the analysis are given in Fig. 21. The loading condition time histories shown in Fig. 21 were derived from an idealization of the Rancho Seco PTS transient. They provide therefore a realistic basis for assessing the potential for clad-tearing initiation under PTS-loading conditions.

Crack-tip stress-intensity factors at the clad-base metal interface (Point 3) on the crack front are shown plotted in Fig. 22. Results are plotted for both elastic and elastic-plastic analyses of the model. Note that elastic-plastic analysis results in a significant reduction in the effective crack-tip stress-intensity factor at this location. The bulk of this reduction is due to plastic deformation in the cladding, since the yield stress for the cladding at the temperature of interest in this analysis is approximately 138 MPa (20. ksi).

Estimated lower-bound K_{Jc} -tearing-toughness curves for cladding, LUS-weld metal and A533B plate material are shown superimposed on the applied K_J curves in Fig. 22. These materials

Fig. 20. A 3-D elastic-plastic finite-element model was used to evaluate the effect of low cladding-tearing toughness on crack initiation under PTS-transient loading. The analysis used a bilinear representation of the stress-strain curve.

Fig. 21. The "Rancho Seco" PTS transient was used in the preliminary evaluation of the effect of low clad-tearing toughness on crack initiation in surface flaws.

Fig. 22. The applied crack tip stress-intensity factor (K_J) at the clad-base metal interface remains significantly below the estimated -2σ -tearing toughness curve for both cladding and LUS weld metal throughout the transient. Plasticity effects significantly reduce the magnitude of the applied K_J at this location (Point 3).

toughness curves were derived from the ductile tearing toughness curves of Fig. 19 by means of (a) expressing the ductile tearing toughness J as an equivalent fracture toughness K_J using the relationship $K_J = \sqrt{E \cdot J}$ and (b) reducing the mean fracture-toughness curves from step (a) by the factor $1/1.43$ to obtain estimated -2σ curves. The factor $1/1.43$ derives from the analysis of the ASME Code fracture-toughness data base reported in Ref. 7. Implicit in the above procedure is the assumption that the ductile tearing toughness curves of Fig. 19 represent the mean tearing toughness of the material.

It is evident from the curves of Fig. 22 that the applied K_I value at the clad-base material interface does not come close to the estimated -2σ -clad-tearing toughness curve at anytime during the transient. This observation holds for the K_I curves generated from both elastic and elastic-plastic analysis results. Based upon the magnitude of the tearing margins derived from the curves of Fig. 22, it appears unlikely that propagation of surface cracks into the base material could be induced by the low ductile-tearing toughness of irradiated stainless steel cladding material.

Evaluation of the effect of low clad-tearing toughness on the response of subclad flaws to PTS loading is to be addressed in the next phase of the task. The configuration of concern is illustrated in Fig. 18 (a). The role of cladding in this configuration is to reduce the crack tip K_I value. Cladding is therefore characterized as having a beneficial impact on the fracture response of subclad flaws to PTS loading. This beneficial effect is retained however only while the cladding above the crack remains integral. Should rupture of the cladding occur, it could result in a relatively rapid increase in the crack tip stress intensity factor. Adverse consequences associated with clad rupture include (a) negating benefits from warm prestressing, (b) delaying the time for crack initiation such that the vessel wall temperature has decreased with an associated reduction in both K_{Ic} and K_{Ia} , and (c) potential reduction in K_{Ic} due to the introduction of strain-rate effects. The significance of the subclad flaw issue has been further emphasized by reports from the French nuclear power program (Ref. 31) of subclad material segregation in ring-forged reactor vessel shell courses.

In addition to its role in assessing the potential for clad-tearing-induced surface propagation of a surface crack, the analysis model of Fig. 20 also provides an insight into the relative potential for ductile tearing and cleavage fracture to trigger crack initiation at the deepest point on the crack front. Results for this point [Point 11] are shown plotted in Fig. 23. The LUS-tearing-toughness curve approaches the applied K_I curve relatively early in the transient at a time when K_I is

Fig.23

Based upon an estimated -2σ tearing toughness curve for LUS material, tearing in advance of cleavage fracture would not be predicted for the transient analyzed. Note that the effect of plasticity on the calculated crack tip stress-intensity factor K_I remains significant at this point (11) on the crack front.

positive. It is conceivable that an alternate transient could result in tearing initiation. The cleavage toughness -2σ curve intersects the applied K_J curve, but does so in a region where K_J is negative. Cleavage-crack initiation should therefore be inhibited by the warm-prestressing effect.

The conclusion drawn from the curves of Fig. 23 is that ductile tearing followed by cleavage may be the most probable mode of crack initiation for vessels containing LUS-weld material. This conclusion is consistent with results obtained in the PTSE-2 thermal-shock tests, in which ductile tearing preceded the onset of cleavage fracture (Ref. 27). These results serve to emphasize the need for timely development and validation of an improved ductile-tearing technology for use in the analysis of PTS transients.

7. Conclusions

HSST program research and development activities described in the previous sections are not yet complete. It follows therefore that only interim conclusions are available at this time. Interim conclusions are of some value however in that they indicate potential directions for PTS analysis technology development. The most significant of these interim conclusions are as follows.

- (a) The input flaw-distribution curve has a dominant influence on the normalized, cumulative crack-initiation curve. By comparison, differences between the dominant transients have second-order effect on the curve.
- (b) The spatial distribution of flaws within the reactor vessel wall has not yet been characterized.
- (c) Limited vessel weld-inspection data indicate that the flaw density in the critical inner region of the vessel weld may be higher than currently assumed by a factor in excess of one order of magnitude.
- (d) The fracture toughness for shallow flaws is expected to be significantly elevated due to the release of crack-tip constraint. Pilot analyses have shown that an elevated shallow-flaw fracture toughness can significantly reduce the predicted number of crack initiations.
- (e) Supplementary correlation parameters can improve the transferability of fracture-toughness data from small-scale specimens to large-scale application. Implications of this development for the fracture analysis of circumferential flaws remain unclear at this time.
- (f) Ductile-tearing prediction remains a problem area in the PTS analysis of LUS vessels. Inclusion of ductile tearing in advance of cleavage fracture appears to be a mandatory requirement for the PTS analysis of reactor vessels containing LUS-weld material.
- (g) Preliminary evaluations indicate that low clad-tearing toughness should not act as a trigger for cleavage initiation in through-clad finite-length flaws.

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