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THE ROLE OF CRACK ARREST IN THE EVALUATION OF PWR PRESSURE VESSEL INTEGRITY DURING PTS TRANSIENTS*

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ABSTRACT

The PWR pressurized thermal-shock (PTS) issue, which is concerned with the integrity of the reactor pressure vessel during postulated overcooling transients, is under intensive investigation by the USNRC. The USNRC-sponsored Integrated Pressurized Thermal-Shock (IPTS) and Heavy-Section Steel Technology (HSST) Programs are dedicated to a better understanding and a timely resolution of the problem.

The HSST program is investigating flaw behavior in large cylinders and is also obtaining fracture-mechanics-related material properties, while the IPTS program is primarily concerned with an estimation of the overall frequency of vessel failure and identification of dominant transients and design and operating features contributing thereto for specific nuclear plants. One important component of the IPTS study is a probabilistic fracture-mechanics analysis of the reactor vessel, and a point of particular interest therein is the role of crack arrest in mitigating the consequences of the postulated PTS transients.

The HSST program has provided crack-arrest data from small specimens and large thermal- and pressure-loaded cylinders that tend to establish the

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validity of the crack-arrest concept for application to the PTS problem. Unfortunately, recent results of the IPTS studies indicate that the inclusion of crack arrest in the probabilistic fracture-mechanics model does not substantially influence the calculated frequency of vessel failure. However, there are still significant questions regarding flaw behavior at upper-shelf temperatures, and the HSST program is continuing to pursue this area of uncertainty.

NOMENCLATURE

a	crack depth
E	modulus of elasticity
h	fluid-film heat-transfer coefficient
J	J integral
K_I	mode-I stress intensity factor
K_{Ia}	static crack-arrest toughness
K_{Ic}	static crack-initiation toughness
K_{IR}	ASME lower bound of dynamic fracture toughness data (also referred to as ASME XI lower-bound K_{Ia})
K_J	critical value of K_I based on J integral, $K_J = \sqrt{J_{material}E}$
p	primary-system pressure
$P(F\|E)$	conditional probability of vessel failure
RTNDT	nil-ductility reference temperature
$RTNDT_o$	initial (zero fluence) value of RTNDT
ΔRTNDT	increase in RTNDT due to radiation damage
t	time in transient
T	temperature
T_D	temperature below which only frangible fracture occurs

T_f	final (asymptotic) temperature of coolant in downcomer
T_i	initial temperature of coolant in downcomer
w	wall thickness
β	exponential decay constant
σ	one standard deviation
$\Phi(E)$	frequency of event (transient)
$\Phi(F)$	frequency of vessel failure

1. INTRODUCTION

The pressurized-water-reactor (PWR) pressurized thermal-shock (PTS) issue has been under intensive investigation by the U.S. Nuclear Regulatory Commission (USNRC) at the Oak Ridge National Laboratory (ORNL) since December 1980, at which time ORNL, as a part of the Heavy-Section Steel Technology (HSST) Program, initiated a PWR pressure-vessel generic fracture-mechanics analysis pertaining to the Rancho Seco transient that occurred on March 20, 1978.[1] The results of this study[2] indicated that primary-system pressure and temperature transients of the type that were believed to have occurred at Rancho Seco could challenge the integrity of some PWR vessels late in life. Studies of transients at other plants followed, and by May 1981 the NRC had established the Integrated Pressurized Thermal-Shock (IPTS) Program at ORNL. An objective of this program was to estimate the frequency of vessel failure for specific PWR plants, and this effort required the postulation of overcooling transients, an estimate of their frequencies, a thermal-hydraulic analysis of these transients to obtain the primary-system pressure and downcomer coolant temperature as a function of time, and finally a probabilistic fracture-mechanics analysis using these pressure and temperature transients as input.

Fracture-mechanics studies conducted some time ago indicated that under PTS loading conditions very shallow preexistent inner-surface flaws could initiate and propagate in a frangible manner, but in some cases crack arrest could be instrumental in preventing excessive penetration of the vessel wall. However, these studies also indicated that in most cases crack arrest would have to take place in a rising K_I field ($dK_I/da > 1$) and/or at temperatures above that corresponding to the onset of the Charpy (CVN) upper shelf, which would require K_{Ia} values higher than had been measured in the lab with small specimens.

Crack-arrest behavior under PTS loading conditions has been under investigation at ORNL as a part of the USNRC-sponsored HSST program, and recent experiments associated therewith have involved arrest in a rising K_I field and arrest on the upper shelf. Furthermore, the IPTS-program specific-plant probabilistic studies, which included detailed analyses for the Oconee-1, Calvert Cliffs-1 and H. B. Robinson-2 nuclear facilities, have been completed. Thus, it is possible and the purpose of this paper to reexamine the role of crack arrest in preventing vessel failure during postulated PTS transients. In doing so, this paper also describes the crack-arrest model that is being used at ORNL for the IPTS studies and reviews relevant crack-arrest data from compact specimens and large test cylinders that indicate the validity of the crack-arrest concept.

2. FRACTURE-MECHANICS MODEL

The probabilistic fracture-mechanics calculations for the IPTS program were performed with the computer code OCA-P,[3] which is based on LEFM and makes use of Monte Carlo methodologies for computing probabilities associated with flaw behavior. Two of the parameters simulated in the probabilistic analysis are the crack-initiation toughness, K_{Ic}, and the crack-arrest toughness, K_{Ia}. Mean values of these parameters were obtained from

$$\overline{K}_{Ic} = 1.43\{36.5 + 3.084 \exp [0.036 (T - RTNDT + 56)]\} \quad (1)$$

$$\overline{K}_{Ia} = 1.25\{29.5 + 1.344 \exp [0.0261 (T - RTNDT + 89)]\} , \quad (2)$$

where

$\overline{K}_{Ic}$, $\overline{K}_{Ia}$ = mean values of initiation and arrest toughness, MPa $\sqrt{m}$;

T = temperature at tip of flaw, °C;

RTNDT = $RTNDT_0$ + ΔRTNDT = nil-ductility reference temperature,[4] °C;

$RTNDT_0$ = initial (zero fluence) value of RTNDT, °C;

ΔRTNDT = increase in RTNDT due to radiation damage, °C.

Equations (1) and (2) represent the ASME Sect. XI lower-bound curves[5] times a constant. The constants were derived by specifying a normal distribution, corresponding standard deviations (σ) of 0.15 $\overline{K}_{Ic}$ and 0.10 $\overline{K}_{Ia}$, and by assuming that the ASME lower-bound curves represent −2σ values. These specific values of σ were based on the data that were used to generate the ASME K_{Ic} and K_{Ia} curves.

A provision is made in OCA-P for limiting K_{Ia} to some maximum value, $(K_{Ia})_{max}$. One way to select a value of $(K_{Ia})_{max}$ is to use a K_J value corresponding to the upper portion of an appropriate J vs Δa curve (J-resistance curve), as illustrated in Fig. 1. This figure shows the radial distribution of fracture toughness through the wall of the vessel at some time during a typical postulated transient. At temperatures less than T_D, it is assumed that the flaw will behave in a frangible manner only; that is, ductile tearing is not permitted following arrest. [Depending on the value of $(K_{Ia})_{max}$

selected, tearing might actually occur and lead to failure.] In accordance with this model, the load line will not intersect the steeply rising portion of the J-Δa curve. Thus, it is sufficient to extend $(K_{Ia})_{max}$ across the T_D line as shown. It is then possible in the analysis for crack arrest to take place on the upper shelf, if the load line rises steeply enough to miss the knee of the K_{Ia} curve and then drops back down again, as it does for some of the postulated PTS transients.

A particular J-Δa curve of interest corresponds to a specific low-upper-shelf weld [referred to as 61W (Ref. 6)] that was irradiated to a fluence of $\sim 1.2 \times 10^{19}$ neutrons/cm^2 at a temperature of ∼290°C and was tested at ∼200°C. The upper portion of the curve is essentially horizontal and equivalent to a K_J value of ∼220 MPa $\sqrt{m}$. This value has been used for $(K_{Ia})_{max}$ in the IPTS studies. As illustrated later, a value of 220 MPa $\sqrt{m}$ nearly corresponds to the CVN onset of upper-shelf temperature; thus, minimal ductile tearing would be expected at lower values.

K_{Ia} data obtained from wide-plate tests in Japan[7] and pressurized thermal-shock experiments with large cylinders at ORNL[8] indicate that values of K_{Ia} substantially greater than 220 MPa $\sqrt{m}$ might be achieved in a PWR vessel. However, the tearing resistance following arrest probably would not be sufficient to prevent ductile tearing up to the point indicated in Fig. 1. Additional experiments are planned as a part of the HSST program to further explore the behavior of flaws under high loading conditions and at temperatures close to and above those corresponding to the onset of upper shelf. Furthermore, calculations have been made to determine the effect of higher values of $(K_{Ia})_{max}$ on the probability of vessel failure. The results of these calculations are discussed later in this paper.

3. VALIDITY OF STATIC CRACK-ARREST CONCEPT FOR PTS STUDIES

The validity of the static crack-arrest concept for application to the PTS issue has been investigated at ORNL by comparing small-specimen crack-arrest data with K_{Ia} values deduced from a series of thermal-shock and pressurized thermal-shock experiments (TSE-4,[9] TSE-5, -5A and -6,[10] PTSE-1[8]). In most cases the lab K_{Ia} data were obtained using 25 x 151 x 151-mm wedge-loaded, compact, crack-arrest specimens; and the source of material was a prolongation from each of the test cylinders. (Battelle Columbus Laboratory was responsible for making the small-specimen K_{Ia} measurements.)

The material used in the testing was a low-alloy forging-grade steel [A508 with class-2 chemistry[11] (see Table 1)] that is used for LWR pressure vessels. Three different heats of material and four different heat treatments were involved. The test cylinders for TSE-5, -5A and -6 came from one heat of material (a single long forging), while the other two test cylinders were from two other heats. The heat treatments differed only in the "tempering" temperature. Preceding tempering, the material was normalized for 8 h at 930°C, air cooled, austenitized for 9 h at 860°C, and then quenched in water. "Tempering" temperatures corresponding to each of the experiments are included in Table 2, which summarizes the test conditions and results. As indicated in the table, test cylinders for four of the five tests (TSE-4, -5, -6, and PTSE-1) were in essentially the quench-only condition. The test cylinder for TSE-5A was tempered at a high enough temperature to nearly satisfy the strength requirements for class-2 material.

Following the water-quench and prior to tempering, sufficient surface material was removed from the test cylinders and their prolongations to avoid having significant residual stresses and gradients in toughness in the sections of material to be tested.[13]

Values of RTNDT for the different test cylinders and their prolongations are also included in Table 2. Because of the different heat treatments and heats of material involved, there is a substantial range in values (10 to 91°C).

A total of 55 small-specimen K_{Ia} values were obtained as a part of the test-cylinder characterization studies,[14,15] and the thermal-shock experiments produced twelve critical values of K_I corresponding to crack-arrest events. The small-specimen K_{Ia} values covered a range of testing temperatures (T − RTNDT) of −66 to +59°C, while the corresponding range for the cylinder tests was −30 to 77°C. The twelve K_{Ia} values deduced from the thermal-shock experiments are plotted in Fig. 2, where they are compared with the ASME Sect. XI K_{Ia} (K_{IR}) curve and the 5 and 95 percentile curves corresponding to a BCL data base of 233 K_{Ia} values for A533 and A508 material. With the exception of a few relatively high data points associated with fracture surfaces having a high density of uncracked ligaments, the BCL 5 and 95 percentile curves represent lower and upper bounds of the 55 K_{Ia} values obtained from the thermal-shock test-cylinder characterization studies (these 55 values are included in the BCL data base).

Also included in Fig. 2 are three K_{Ia} values deduced from a French thermal-shock experiment[16] and five values obtained by the Japanese from wide-plate tests,[7] which, like the ORNL pressurized thermal-shock experiment (PTSE-1),[8] permits the investigation of crack arrest at relatively high temperatures.

As indicated by Fig. 2, the scatter in the small-specimen data, with the excpetion of the few outliers mentioned above, is ∿±45% over the temperature range shown, and only a small fraction of the data points falls below the K_{IR} curve. All of the K_{Ia} values deduced from the ORNL thermal-shock experiments fall within the small-specimen scatter band and well above the K_{IR} curve, while the three French data points cluster about the K_{IR} curve.

The first crack-arrest events during TSE-5A and TSE-6 and both crack-arrest events during PTSE-1 took place in a rising K_I field ($dK_I/da > 0$), and it is not evident that this resulted in K_{Ia} values different from those obtained with $dK_I/da < 0$, which is the usual condition when obtaining small-specimen K_{Ia} values. Because of this and the good agreement between the small-specimen and large-cylinder data, it appears that the LEFM crack-arrest concept based on small-specimen data is reasonably valid and can be used with confidence in evaluating the integrity of PWR pressure vessels during postulated PTS transients. Furthermore, the data in Fig. 2 indicate that it would be appropriate to use a higher mean curve in the IPTS probabilistic fracture-mechanics analyses than that expressed by Eq. (2). In the next section of this paper the effect of using a higher value is discussed.

With reference to Fig. 2, it is also of interest to note that the CVN onset of upper-shelf temperature corresponds to a mean value of K_{Ia}, based on the large-specimen data, of nearly 200 MPa $\sqrt{m}$. This indicates that little, if any, ductile tearing would take place at K_I values less than the $(\overline{K_{Ia}})_{max}$ values selected for the IPTS studies.

4. BEHAVIOR OF INNER-SURFACE FLAWS DURING PTS TRANSIENTS

A fracture-mechanics analysis of the Oconee-1[17] vessel, based on the model described earlier, indicates that for some postulated high-pressure transients axially oriented inner-surface flaws with a maximum surface length of ∿2 m (height of a shell course) will propagate through the vessel wall without arresting, as indicated by the set of critical-crack-depth curves in Fig. 3.* Furthermore, increasing the value of $(K_{Ia})_{max}$ above 220 MPa $\sqrt{m}$ does not affect this result, provided that Eq. (2) is appropriate for extrapolating to higher values of K_{Ia}. For an increase in $(K_{Ia})_{max}$ to have an ameliorating effect, the K_{Ia} vs T curve would have to turn up more abruptly.

For a somewhat more severe transient than that associated with Fig. 3 it is possible, as shown in Fig. 4, for crack arrest to take place, if $(K_{Ia})_{max}$ is increased above 220 MPa $\sqrt{m}$. However, the arrest event would take place at or above a temperature corresponding to the onset of the Charpy upper shelf, and thus ductile tearing would be expected as indicated in Fig. 5. If ductile tearing did not take place and if the particular transient extended for ∿30 min or more beyond the minimum critical time (∿35 min), the propagated flaw would reinitiate, and presumably arrest would not take place, as indicated in Fig. 4. Furthermore, if the transient extended as little as ∿10 min or more beyond the minimum critical time, much shallower flaws could propagate without arresting. Thus, for this transient also, increasing $(K_{Ia})_{max}$ does not prevent failure, at least for the set of

*The initiation and arrest curves ($K_I = K_{Ic}$, $K_I = K_{Ia}$) in Figs. 3 and 4 are based on the ASME Sect. XI K_{Ic} and K_{Ia} curves, and no maximum values were imposed. The inclusion of the 220 MPa $\sqrt{m}$ iso-K_I curve allows interpretation of the results as if this maximum value were imposed.

conditions assumed for the particular analysis regarding fluence, fracture-toughness and copper and nickel concentrations.

In the probabilistic fracture-mechanics analysis, parameters that have significant uncertainties associated with them are simulated, and this results in a rather broad range of predicted flaw behavior for a given transient. It is not feasible to examine each of the thousands of cases involved in the manner described above, but it is possible and instructive to perform the probabilistic fracture-mechanics analysis using different values of $(K_{Ia})_{max}$ in conformance with the arrest model described in Fig. 1. A comparison can then be made between the calculated values of the total frequency of failure, $\Phi(F)$, which is obtained from

$$\Phi(F) = \sum_i \Phi(E)_i \, P(F|E)_i \ ,$$

where

$\Phi(E)_i$ = frequency of the ith event (transient),

$P(F|E)_i$ = probability of vessel failure for the ith event.

Values of $P(F|E)_i$ are obtained from the probabilistic fracture-mechanics analysis.

A study of this type was conducted for each of the specific nuclear plants, and the results indicated that increasing $(K_{Ia})_{max}$ from 220 to 330 MPa $\sqrt{m}$ decreased $\Phi(F)$ by a factor of less than two for each of the plants.

As mentioned earlier, the data in Fig. 2 suggest that a higher mean curve for $K_{Ia} = f(T - RTNDT)$ could be used in the IPTS studies. Figure 6, which is a slight variation of Fig. 2 for illustrative purposes, indicates that a better fit to the data than expressed by Eq. (2) is achieved with

$$\overline{K}_{Ia} = 1.75 \ K_{IR} \ . \qquad (4)$$

The effect of using Eq. (4) with $2\sigma = 0.45\ \overline{K}_{Ia}$ instead of Eq. (2) with $2\sigma = 0.20\ \overline{K}_{Ia}$ was to reduce $\Phi(F)$ by factors of only 2, 4 and 6 for Oconee-1, Calvert Cliffs-1 and H. B. Robinson-2, respectively.

It was expected that the lower the pressure and the less severe the thermal shock the greater the effect of crack arrest on $P(F|E)$. Of course it may be a trivial matter since it seems unlikely that the less severe transients will be dominant. Even so, the influence of the severity of the transient on the effect of increasing $\overline{K}_{Ia}$ and $(K_{Ia})_{max}$ by the amounts indicated above was investigated for transients characterized by a constant pressure (p) and an exponential decrease in coolant temperature. An initial temperature of 288°C, a decay constant of 0.15 min^{-1}, several asymptotic temperatures (T_f) and a range of pressures from 3 to 17 MPa were included in the analysis. The results indicated, as before, essentially no increase in $P(F|E)$ due to an increase in $(K_{Ia})_{max}$ from 220 to 330 MPa $\sqrt{m}$.

As indicated in Fig. 7, the decrease in $P(F|E)$ due to the specified increase in $\overline{K}_{Ia}$ and its uncertainty tends to increase with decreasing severity of the transient. However, for $p \geq 7$ MPa and $T_f \leq 120°C$, the sensitivity is small (factor of 3 or less). For less severe transients the sensitivity can be substantially greater, but these transients are not likely to be dominant.

Although crack arrest does not appear to play an important role in the evaluation of vessel integrity during postulated PTS transients, there is still considerable uncertainty regarding flaw behavior, particularly crack arrest, at temperatures close to and corresponding to the upper shelf. The HSST program is continuing to investigate flaw behavior in this temperature regime.

5. SUMMARY

Crack-arrest data (K_{Ia}) deduced from thermal-shock experiments and wide-plate tests agree very well with lab small-specimen data and generally lie above the ASME Sect. XI K_{IR} curve. The comparison extends over the temperature range (T – RTNDT) of –32 to +93°C, and the scatter in the data up to ∼60°C is such that $2\sigma \cong \pm 0.45\ \overline{K}_{Ia}$.

The mean of the above data is about 1.75 times the ASME Sect. XI K_{IR} curve. This mean curve is substantially higher than the mean curve selected for the IPTS studies on the basis of the small-specimen data that were used to establish the K_{IR} curve ($K_{Ia} = 1.75\ K_{IR}$ compared to $1.25\ K_{IR}$). Unfortunately, inclusion of the higher mean curve in the IPTS studies does not reduce the calculated frequency of vessel failure, $\Phi(F)$, by a substantial amount.

The ORNL PTSE-1 K_{Ia} data and the Japanese wide-plate K_{Ia} data indicate that K_{Ia} values greater than the maximum value used in the IPTS studies (220 MPa $\sqrt{m}$) might actually be achieved. However, the IPTS studies indicate that $\Phi(F)$ is insensitive to increases in $(K_{Ia})_{max}$ up to at least 330 MPa $\sqrt{m}$.

The IPTS studies indicate that the effect on $\Phi(F)$ of increasing $\overline{K}_{Ia}$ and $(K_{Ia})_{max}$ is transient dependent and thus imply that the effect might be greater for other nuclear plants. Furthermore, there are still significant uncertainties regarding crack arrest on the upper shelf. Thus, the HSST program is continuing to investigate crack-arrest phenomenon for PTS loading conditions.

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Table 1. Chemical composition for A508 class 2 material[11]

Table 2. Test conditions and summary of results for ORNL thermal-shock and pressurized thermal-shock experiments

Composition,[a] weight percent								
C	Mn	P	S	Si	Cr	Ni	Mo	V
0.27	0.50–1.00	0.012	0.015	0.15–0.40	0.25–0.45	0.50–1.00	0.55–0.70	0.05

[a]Single values are maximum.

	TSE-4	TSE-5	TSE-5A	TSE-6	PTSE-1
Cylinder dimensions, mm					
Outer diameter	533	991	991	991	991
Inner diameter	242	686	686	838	686
Length	914	1219	1219	1219	1372
Cylinder material					
Type	← SA 508 with class-2 chemistry →				
Tempering temperature, °C	No tempering[a]	613[b]	679[b]	613[b]	561[c]
RTNDT, °C	~90[d]	66[e]	10[f]	66[e]	91[e]
Flaw (initial)					
Type	← Long, inner surface →				Long, outer surface
Orientation	← Longitudinal →				
Length, mm	914	1219	1219	1219	1000
Depth, mm	11	16	11	8	11
Thermal shock					
Cylinder initial temperature, °C	291	96	96	96	290
Coolant initial temperature, °C	−25	−196	−196	−196	−25
Coolant	Alcohol and water	LN_2	LN_2	LN_2	Alcohol and water
Pressure, MPa	0	0	0	0	0–90
Initiation-arrest events					
Number	1	3	4	2	2
Arrest depth, mm	22	30, 96, 122	21, 30, 48, 82	21, 142	22, 37
Arrest temperature, °C					
T	126	36, 82, 89	22, 38, 51, 67	34, 64	157, 169
T − RTNDT	36	−30, 16, 23	12, 28, 41, 57	−32, −2	66, 78
K_{Ia}, MPa$\sqrt{m}$	127	86, 104, 92	76,[g] 86, 107, 130	63,[g] 105	177,[g] 265[g]

[a]Cylinder tested in quenched condition.
[b]Temperature maintained for 4 h, air cooled.
[c]Stress-relief treatment only.
[d]Corresponds to CVN energy of 40 J. No drop-weight testing; CVN upper shelf less than 68 J.
[e]Based on CVN data.[12]
[f]Based on drop weight.[12]
[g]Rising K_I field.

FIGURE CAPTIONS

Fig. 1. Illustration of method of selecting $(K_{Ia})_{max}$.

Fig. 2. Comparison of small-specimen and large-specimen K_{Ia} data.

Fig. 3. Critical-crack-depth curves, corresponding to 32 EFPV, for Oconee-1 postulated transient No. 44.

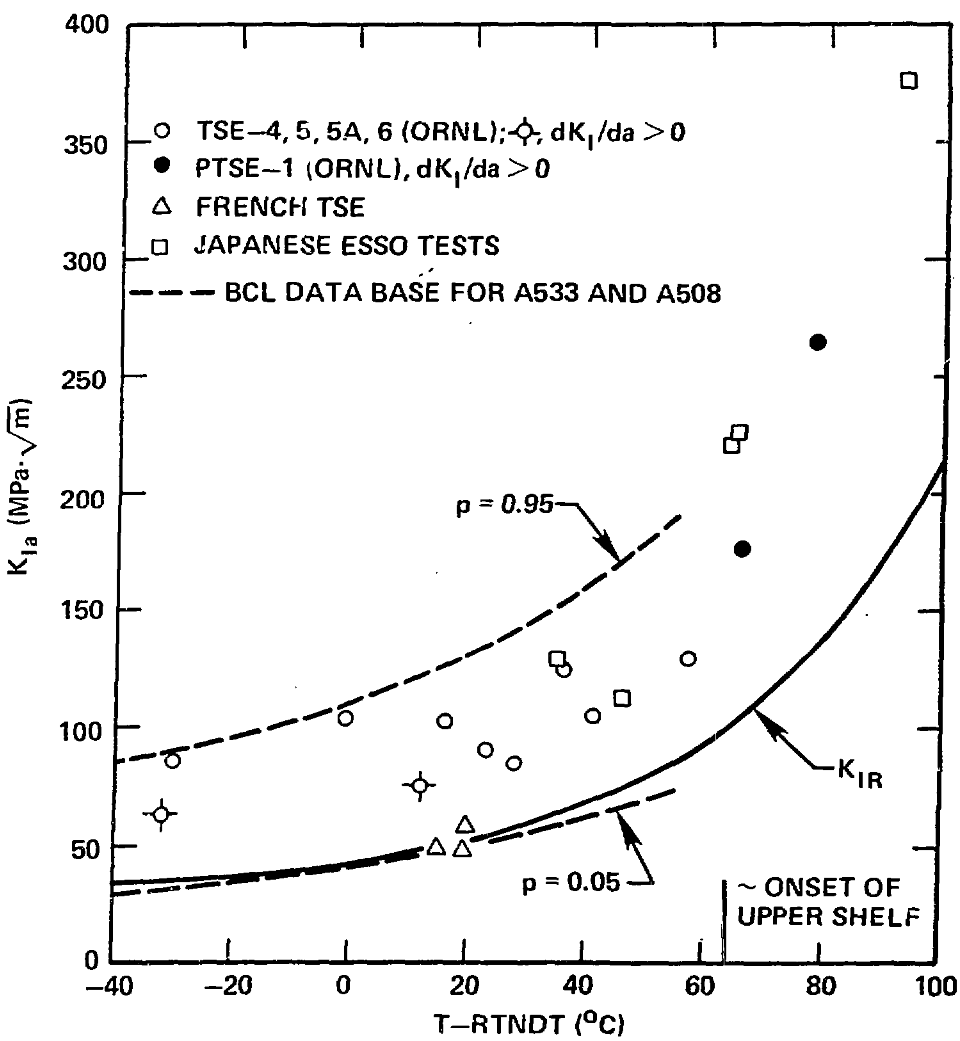
Fig. 4. Critical-crack-depth curves, corresponding to 32 EFPY, for Oconee-1 postulated transient No. 34.

Fig. 5. Radial distribution of K_I and K_{Ia} in vessel wall at a specific time in a PTS transient.

Fig. 6. Comparison of large-specimen K_{Ia} data and $\overline{K}_{Ia}$ = f(T–RTNDT) curves used in IPTS studies.

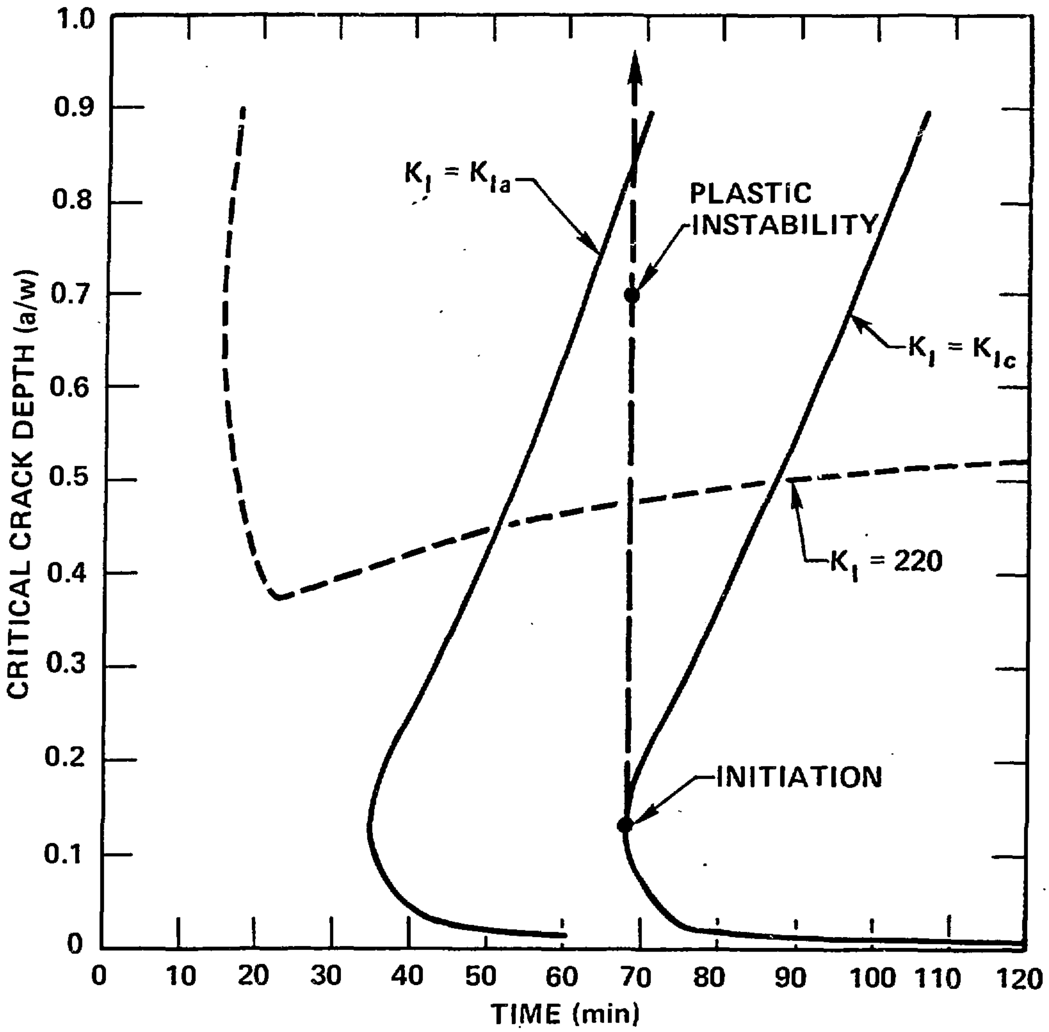
Fig. 7. Sensitivity of P(F|E) to change in $\overline{K}_{Ia}$ from 1.25 K_{IR} (2σ = 0.20 $\overline{K}_{Ia}$) to 1.75 K_{IR} (2σ = 45 $\overline{K}_{Ia}$) for transients characterized by constant pressure and exponential decay of coolant temperature.

F2

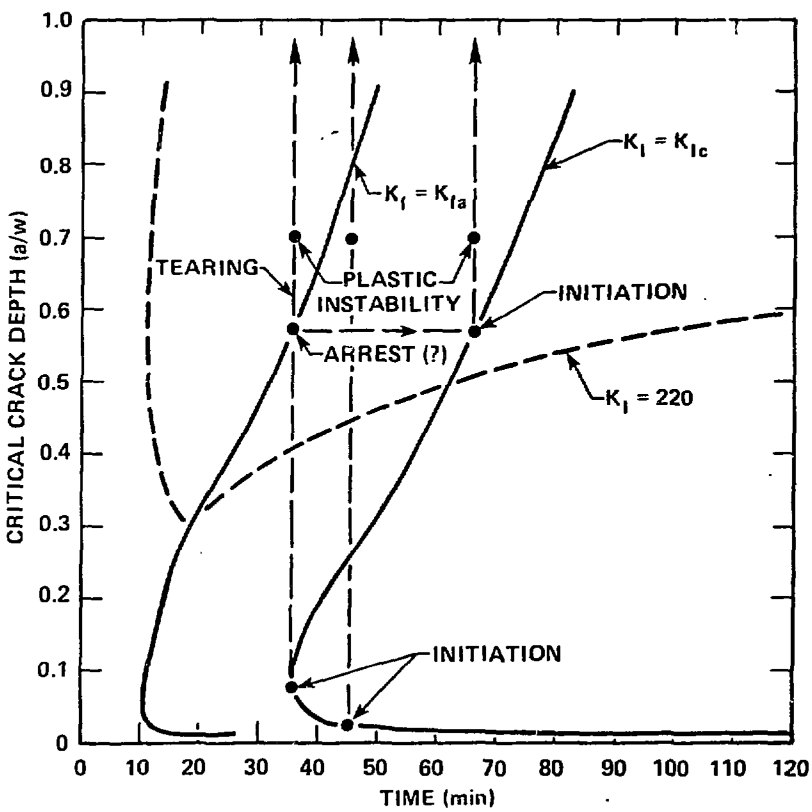


400
350
300
250
200
150
100
50
0
−40
−20
0
20
40
60
80
100
T−RTNDT (°C)
K_Ia (MPa·√m)
TSE−4, 5, 5A, 6 (ORNL); dK_I/da > 0
PTSE−1 (ORNL), dK_I/da > 0
FRENCH TSE
JAPANESE ESSO TESTS
BCL DATA BASE FOR A533 AND A508
p = 0.95
p = 0.05
K_IR
~ ONSET OF
UPPER SHELF

F3

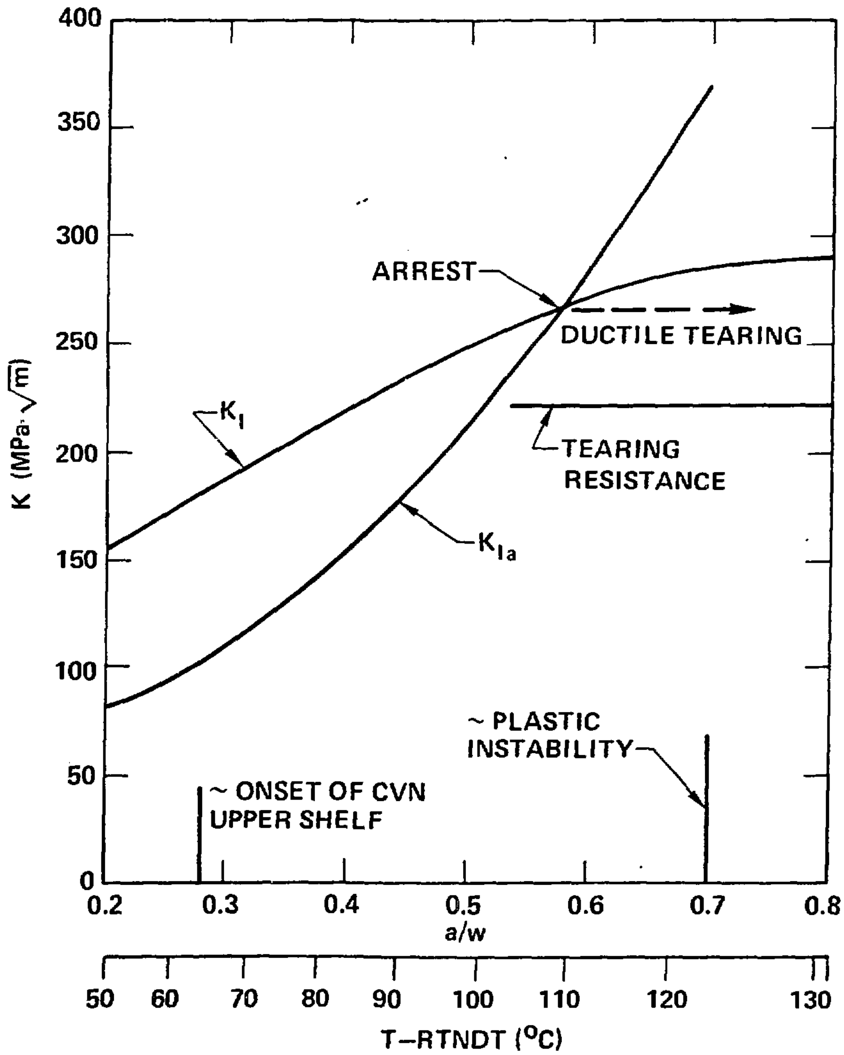

1.0
0.9
0.8
0.7
0.6
0.5
0.4
0.3
0.2
0.1
0
CRITICAL CRACK DEPTH (a/w)
$K_I = K_{Ia}$
PLASTIC INSTABILITY
$K_I = K_{Ic}$
$K_I = 220$
INITIATION
0
10
20
30
40
50
60
70
80
90
100
110
120
TIME (min)

F4



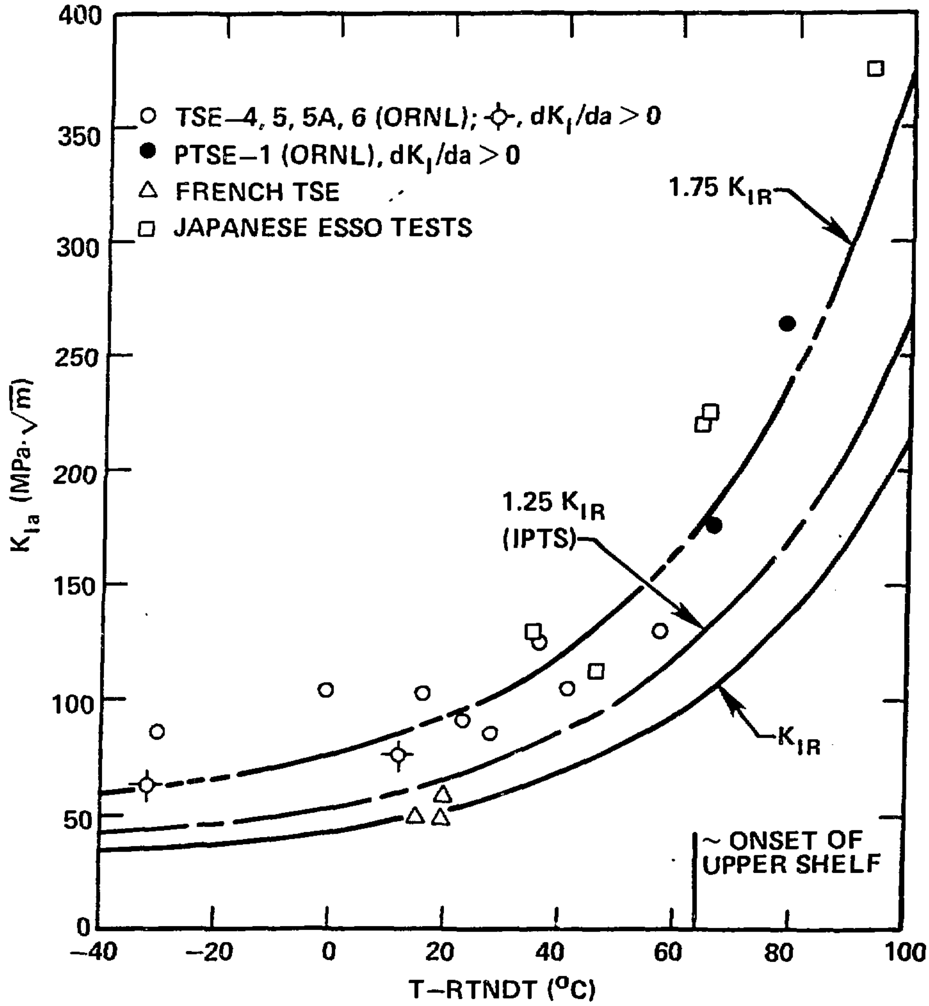
CRITICAL CRACK DEPTH (a/w)
TIME (min)
1.0
0.9
0.8
0.7
0.6
0.5
0.4
0.3
0.2
0.1
0
0 10 20 30 40 50 60 70 80 90 100 110 120
$K_I = K_{Ic}$
$K_I = K_{Ia}$
TEARING
PLASTIC INSTABILITY
INITIATION
ARREST (?)
$K_I = 220$
INITIATION

F5



400
350
300
250
200
150
100
50
0
K (MPa·√m)
ARREST
DUCTILE TEARING
K_I
TEARING RESISTANCE
K_Ia
~ PLASTIC INSTABILITY
~ ONSET OF CVN UPPER SHELF
0.2
0.3
0.4
0.5
0.6
0.7
0.8
a/w
50
60
70
80
90
100
110
120
130
T–RTNDT (°C)

F6



TSE—4, 5, 5A, 6 (ORNL); dK_I/da > 0
PTSE—1 (ORNL), dK_I/da > 0
FRENCH TSE
JAPANESE ESSO TESTS
1.75 K_IR
1.25 K_IR (IPTS)
K_IR
~ ONSET OF UPPER SHELF
K_Ia (MPa·√m)
T—RTNDT (°C)
400
350
300
250
200
150
100
50
0
−40
−20
0
20
40
60
80
100

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