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THE INTEGRITY OF PWR PRESSURE VESSELS DURING OVERCOOLING ACCIDENTS*

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ABSTRACT

The reactor pressure vessel in a pressurized water reactor is normally subjected to temperatures and pressures that preclude propagation of sharp, crack-like defects that might exist in the wall of the vessel. However, there is a class of postulated accidents, referred to as overcooling accidents, that can subject the pressure vessel to severe thermal shock while the pressure is substantial. As a result of such accidents vessels containing high concentrations of copper and nickel, which enhance radiation embrittlement, may possess a potential for extensive propagation of preexistent inner surface flaws prior to the vessel's normal end of life.

For the purpose of evaluating this problem a state-of-the-art fracture-mechanics model was developed and has been used for conducting parametric analyses and for calculating several recorded PWR transients. Results of the latter analysis indicate that there may be some vessels that have a potential for failure in a few years if subjected to a Rancho Seco-type transient. However, the calculational model may be excessively conservative, and this possibility is under investigation.

INTRODUCTION

The reactor pressure vessel in a pressurized water reactor (PWR) is normally subjected to temperatures and pressures that preclude propagation of sharp, crack-like defects (flaws) that might exist in the wall of the vessel. However, there is a class of postulated accidents, referred to as overcooling accidents (OCA's), that allow cool water to come in contact with the inner surface of the vessel wall, resulting in high thermal stresses and a reduction in fracture toughness near the inner surface. This introduces the possibility of propagation of preexistent inner-surface flaws, and this possibility increases with reactor operating time because of the additional reduction in fracture toughness that results from exposure of the vessel material to fast neutrons.

Thermal loading (thermal shock) by itself presumably cannot drive a flaw all the way through the wall; however, if the primary-system pressure is substantial, a

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potential for vessel failure could exist; that is, a preexistent flaw, under proper circumstances, could penetrate the vessel wall and provide a large enough opening to prevent flooding of the reactor core. The nuclear industry has been aware of this problem for quite some time,[1,2,3] but the probability of the existence of the requisite conditions for significant flaw propagation seemed very remote. In recent years however, several PWR OCA initiating events have occurred,[4,5,6] and there has also been a growing awareness that copper and nickel significantly enhance radiation damage in the vessel.[7,8] As a result a reevaluation of the integrity of PWR pressure vessels during OCA's has been undertaken.

A complete evaluation of the OCA problem in terms of its threat to pressure vessel integrity requires consideration of a number of factors, including postulated accident initiating events, reactor system and operator response to these events, specific design features of the reactor vessel and core that affect fluence-rate and coolant-temperature distributions adjacent to the inner sufrace of the vessel wall, sensitivity of the vessel material to radiation damage, size and orientation of preexistent flaws, and remedial measures. This paper examines primarily the fracture-mechanics-related conditions that could lead to a potential for vessel failure.

THE TENDENCY FOR INNER-SURFACE FLAWS TO PROPAGATE DURING THERMAL-SHOCK LOADING ONLY

The tendency for inner-surface flaws to propagate as a result of thermal-shock loading is illustrated in Fig. 1, which shows the temperature, resultant thermal stress, and fracture toughness distributions through the wall of the vessel (exclusive of cladding) at a particular time during a postulated large-break loss-of-coolant accident (LBLOCA). Also included in the figure for the same time in the transient are the stress intensity factors (K_I) for long axial flaws of different depths and the radial distribution of the fast neutron fluence. As indicated, the positive gradient in temperature and the steep attenuation of the fluence result in positive gradients in the crack initiation toughness (K_{Ic}) and the crack arrest toughness (K_{Ia}), and these positive gradients tend to limit crack propagation. However, K_I for the assumed long axial flaw also increases with flaw depth, except near the back surface, and for the particular case and time analyzed it is evident that both shallow and deep flaws can initiate; that is, $K_I \geq K_{Ic}$ for a broad range of crack depths. As the crack tip moves through the wall it encounters higher toughness material and for this particular case eventually arrests.

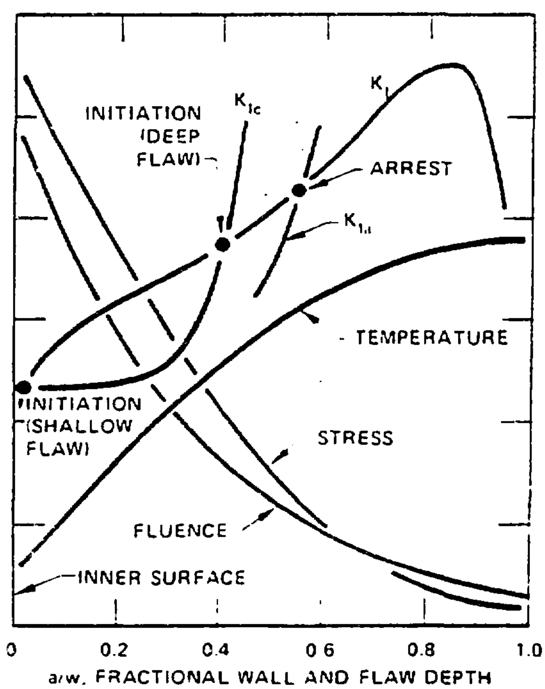

Fig. 1. Radial distributions in a vessel wall of several fracture-mechanics-related parameters at a specific time during a PWR LOCA.

If the crack depths corresponding to the initiation and arrest events are plotted as a function of the times in the transient at which the events take place, a set of curves referred to as the critical-crack-depth curves is obtained that indicates the behavior of the flaw during the entire transient. A typical set of critical-crack-depth curves for a LBLOCA is shown in Fig. 2. As indicated by the dashed lines the long axial flaw would propagate in a series of initiation-arrest events and, if a phenomenon referred to as warm prestressing (WPS) were not effective, would penetrate deep into the wall.

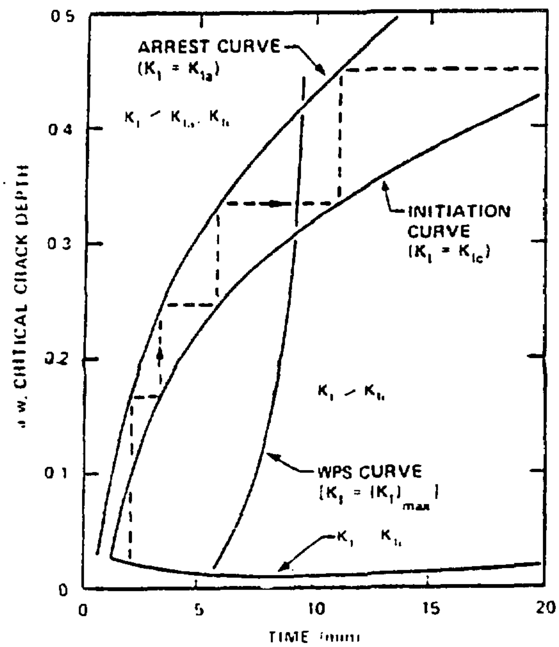

Fig. 2. Critical-crack-depth curves for a PWR LOCA assuming a long axial flaw, high concentrations of copper and nickel, and normal end-of-life fluence.

Warm prestressing, as referred to above, is a term used to describe a situation where K_I is decreasing with time (t) when K_I becomes equal to K_{Ic} by virtue of a decrease in temperature. It has been postulated[9] and demonstrated experimentally[9,10] that under these conditions a flaw will not propagate; that is, a flaw will not initiate while K_I is decreasing. In Fig. 2 the WPS curve is the locus of points for $K_I = (K_I)_{max}$ ($dK_I/dt = 0$). To the left of the WPS curve $dK_I/dt > 0$ and thus crack initiation can take place, but to the right of the WPS curve $dK_I/dt < 0$, and crack initiation will not take place. For the particular case illustrated in Fig. 2, WPS limits crack propagation to ~40% of the wall thickness.

Even if WPS were not effective, the flaw could not completely penetrate the wall under thermal-shock loading conditions only. This is a result of the substantial decrease in K_I as the crack tip approaches the outer surface (see Fig. 1) and has been demonstrated recently in a thermal-shock experiment.[11] However, when pressure is applied in addition to the thermal loading, the possibility of vessel failure (complete penetration of the wall) exists for some assumed conditions.

FRACTURE MECHANICS CALCULATIONAL MODEL

Linear elastic fracture mechanics (LEFM)[12] has been used thus far to analyze the behavior of a flaw during the postulated overcooling accidents. The initial flaw was assumed to be quite long on the vessel surface, to be oriented either in an axial or circumferential direction and to extend radially through the cladding into the base material. The thin layer of stainless steel cladding on the inner surface was included as a discrete region, in which case its effect on temperature and stress and thus K_{Ic}, K_{Ia}, and K_I were accounted for.

Fracture toughness data (K_{Ic} and K_{Ia} vs T − RTNDT, where T is the temperature and RTNDT is the reference nil ductility temperature) were taken from ASME Section XI,[13] and the reduction in toughness due to radiation damage was estimated using Eq. 1, which was recently proposed (tentatively) by Randall[8] as a revision to Reg. Guide 1.99, Rev. 1.[14]

$$\Delta RTNDT = f\ (Cu,\ Ni,\ F) \propto (F)^{0.27}\ , \tag{1}$$

where

$2 \times 10^{17} \leq F \leq 6 \times 10^{19}$ neutrons/cm^2 ,

ΔRTNDT = change in RTNDT at tip of flaw due to fast neutron exposure,

Cu, Ni = copper and nickel concentrations, wt %

F = fast neutron fluence ($E \geq 1$ MeV) at tip of flaw

A typical attenuation of the fluence through the wall of the vessel that includes a correction for the effect of displaced atoms (DPA) on radiation damage was also recently proposed by Randall[8] and is being used in the ORNL studies. The relation is

$$F = F_o e^{-0.0094a\ mm^{-1}} , \quad (2)$$

where

F = fast neutron fluence at tip of flaw

F_o = fast neutron fluence at inner surface of vessel

a = depth of flaw

It is of interest to note that the use of Eq. 1 as opposed to Reg. Guide 1.99, Rev. 1, and the inclusion of the effects of DPA in the fluence attenuation equation result in relatively greater estimated values of radiation damage (ΔRTNDT) deep in the wall of the vessel.

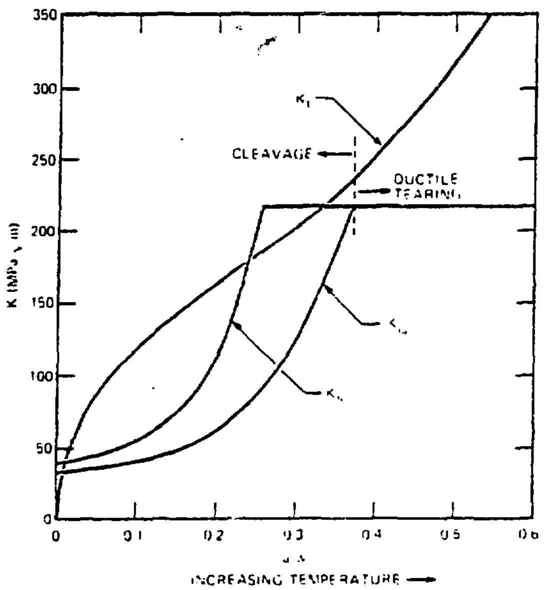

Fig. 3. Plots of K_I, K_{Ic} and K_{Ia} vs fractional crack depth at a specific time in an OCA transient, indicating initiation but no arrest unless on the upper shelf.

For some postulated OCA's, following crack initiation the tip of the fast-running crack will encounter upper-shelf-toughness temperatures prior to crack arrest, as illustrated in Fig. 3. Since techniques are not yet well established for evaluating flaw behavior under these conditions, it was assumed that crack arrest would not occur if K_I was above an arbitary upper-shelf toughness value of 220 MPa $\sqrt{m}$ prior to a calculated arrest event.

The procedure used for evaluating the integrity of a pressure vessel was to calculate, using the above model, the threshold or critical values of RTNDT corresponding to incipient initiation (II) of a flaw and incipient failure (IF) of the vessel (extension of the flaw through the wall) and then compare these critical values with the estimated actual values for a particular PWR pressure vessel. To obtain the critical values of RTNDT it is necessary to specify a transient, the fracture-mechanics model, a failure criterion and an initial (zero fluence) value of RTNDT ($RTNDT_o$), although the results are not very sensitive to the latter parameter. To obtain the actual value of RTNDT for a specific plant it is necessary to have a consistent set of values for the fluence, Cu, Ni and $RTNDT_o$ that corresponds to an area of the vessel wall that is most likely to experience propagation of a flaw; that is, the area in which the worst combination of the four parameters exists.

For convenience the particular values of RTNDT that are compared with each other are the values corresponding to the inner surface of the vessel wall, using material properties for the base material rather than for the cladding. These values of RTNDT are referred to herein as $(RTNDT_S)_C$, the critical value, and $(RTNDT_S)_A$, the actual value.

The critical value of RTNDT is the minimum value, with respect to both time in the transient and crack depth, that results in $K_I = K_{Ic}$ and/or crack penetration of the wall (no arrest). Since $K_{Ic} = f\ (T, RTNDT_o, \Delta RTNDT)$ only,[13] where T is the temperature at the crack tip, it is only necessary to determine these three parameters and K_I to perform the analysis. Values of ΔRTNDT are calculated from Eq. 3, which was obtained by combining Eqs. 1 and 2.

$$\Delta RTNDT = \Delta RTNDT_s \; e^{-2.54 \times 10^{-3} a \ mm^{-1}} \tag{3}$$

The complete analysis for obtaining $(RTNDT_s)_c$ was performed with the computer code OCA-II,[15] which accepts as input the downcomer-coolant-temperature and primary-system-pressure transients and automatically searches for $(\Delta RTNDT_s)_c$. For some OCA's $(\Delta RTNDT_s)_c$ corresponds to incipient initiation followed by crack arrest and no reinitiation, as shown in Fig. 4 assuming WPS to be ineffective. However, increasing $\Delta RTNDT_s$ will eventually result in failure (no arrest), and the corresponding minimum value is $(\Delta RTNDT_s)_c$ for incipient failure. For other OCA's, $(\Delta RTNDT_s)_c$ corresponds to both incipient initiation and incipient failure because, as shown in Fig. 5, there is no arrest following initiation of a shallow flaw. This latter situation tends to be typical of high-pressure transients and the former of low-pressure transients.

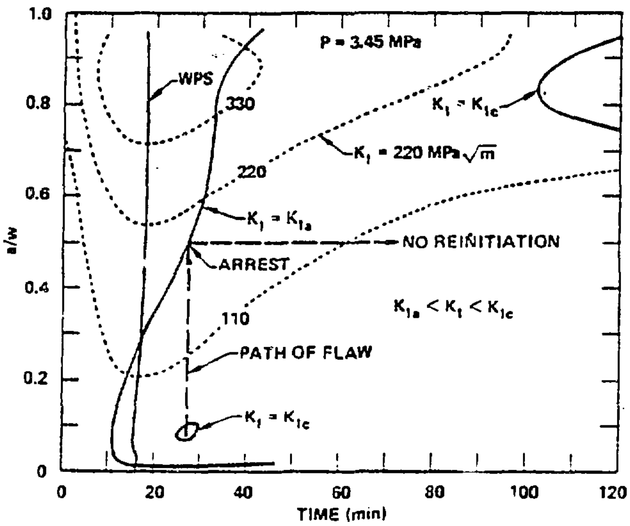

Fig. 4. Critical-crack-depth curves for an OCA illustrating incipient initiation followed by arrest and no reinitiation.

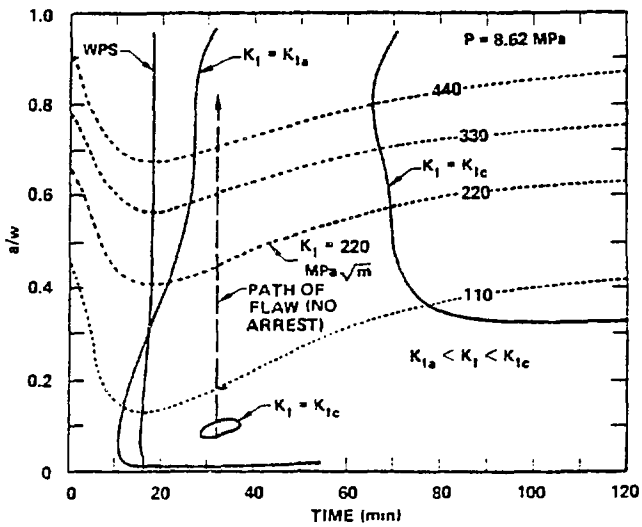

Fig. 5. Critical-crack-depth curves for an OCA illustrating incipient initiation and failure (no arrest unless on the upper shelf).

The sets of critical-crack-depth curves in Figs. 4 and 5 include the locus of points for constant values of K_I. This allows one to determine if arrest takes place in accordance with a maximum specified value for K_{Ia} (220 MPa $\sqrt{m}$ for these studies). In Fig. 4 it does and in Fig. 5 it does not. [The initiation and arrest curves in Figs. 4 and 5 were extended beyond points corresponding to existing maximum values for K_{Ic} and K_{Ia} (~200 MPa $\sqrt{m}$) using the K_{Ic} and K_{Ia} equations in Ref. 13 for extropolation purposes; thus, the extensions of the initiation and arrest curves beyond these points are fictitious to some extent but nevertheless allow one to apply different upper-shelf toughness values when using the critical-crack-depth curves to evaluate flaw behavior.]

The existence of two initiation loops (locus of points for $K_I = K_{Ic}$) in Figs. 4 and 5 suggests additional criteria for calculating $(\Delta RTNDT_s)_c$. One is a reasonable range of depths for initial flaws, and the other is the duration of the transient (t_{max}). For the cases depicted by Figs. 4 and 5, specification of a maximum initial fractional flaw size of 0.15 made a difference, because for lower values of $\Delta RTNDT_s$ the small initiation loop (actually just a point for incipient initiation) would disappear, and $(\Delta RTNDT_s)_c$ would be determined by the other initiation loop in accordance with some other criteria such as a greater critical flaw depth.

EVALUATION OF THE FM MODEL

The validity of LEFM for application to thermal-shock problems has been verified in a series of thermal-shock experiments with thick-walled steel cylinders.[10,11,16] These experiments were designed to exhibit flaw behavior trends calculated to exist during OCA's and thus included initiation and arrest of long axial shallow and deep flaws, a stepwise progression of the flaw deep into the wall, arrest in a rising K_I field ($dK_I/da>0$) and WPS with $dK_I/dt<0$. There are still some areas of uncertainty, but in each of these areas the FM model described above is believed to be conservative. The degree of conservatism is not known at this time, but programs are underway to obtain such information. The presumed conservative features in the model include (1) consideration of long flaws that extend through the cladding, (2) no arrest on the upper shelf, and (3) to some extent a disregard for the beneficial effects of warm prestressing. Long surface flaws have a greater potential than others for penetrating deep into the wall, but the probability of a long flaw existing as an initial flaw and of any length flaw extending through the cladding presumably is very small. One justification for assuming long flaws was that under thermal-shock loading conditions and in the absence of cladding short flaws tend to extend on the surface to become long flaws.[17] However, it may be that the cladding will prevent short flaws from extending on the surface and if so would limit radial growth of the flaw.[18]

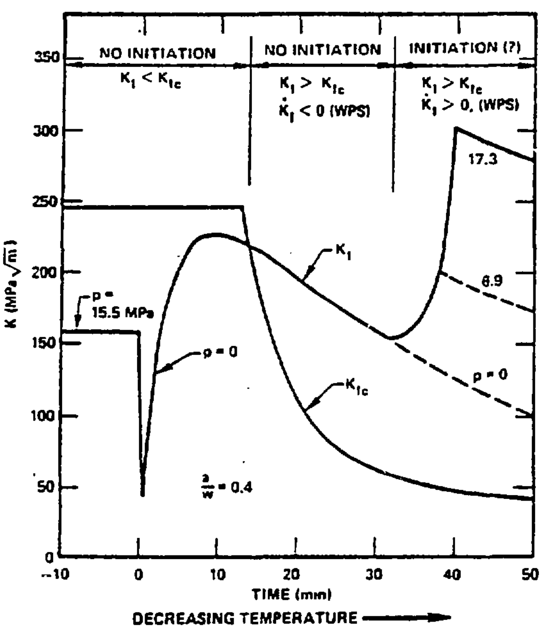

Fig. 6. Illustration of an OCA transient involving repressurization and two types of WPS.

If long flaws through the cladding must be considered, there is still the possibility that the tearing resistance of the material will be sufficient to permit arrest on the upper shelf, and it is also possible that WPS effects in addition to the one mentioned earlier will help to limit flaw propagation. For instance, Fig. 6, which compares K_I and K_{Ic} for a particular crack depth during a postulated transient involving loss of pressure and then repressurization, indicates two types of WPS. During normal operation of the reactor ($t<o$), the material toughness corresponds to upper shelf conditions and K_I is relatively low, as indicated. The transient starts at time zero, and as it progresses K_I becomes equal to K_{Ic}, but only after K_I has begun to decrease with time. Thus, crack initiation would not take place even though K_I becomes substantially greater than K_{Ic}. When repressurization finally takes place, K_I increases with time again, but WPS experiments conducted by Loss, Grey and Hawthorne[9] indicate that because of the particular thermal and loading history that the stationary flaw was exposed to the effective value of K_{Ic} would be elevated, perhaps to a value equal to the previous maximum value of K_I. Thus, presumably some repressurization would be possible, but this particular beneficial effect of WPS was not included in the FM model. (There is some hesitancy at this time to take advantage of WPS even with $dK_I/dt<0$ because there is no assurance that dK_I/dt will remain negative.)

OCA PARAMETRIC ANALYSIS

To obtain a better understanding of the sensitivity of $(RTNDT_s)_c$ to the many parameters involved in an OCA FM analysis, a parametric study was conducted, assuming a constant pressure and an exponential decay of the downcomer coolant temperature.

The temperature transient is expressed as

$$T_c = T_f + (T_i - T_f)e^{-nt} \quad , \tag{4}$$

where

T_c = downcomer coolant temperature,

T_i = initial temperature of vessel wall and coolant,

T_f = final (asymptotic) temperature of coolant,

n = decay constant,

t = time in transient.

The fluid-film heat transfer coefficient (h_f) which is a necessary input to OCA-II, was assumed to be independent of time and for most cases was assigned a value that is achieved with the main circulating pumps running (5680 $W \cdot m^{-2} \cdot °C^{-1}$). In order to determine the sensitivity of $(RTNDT_s)_c$ to h_f a relatively low value corresponding to natural convection cooling (1700) was also used for a few calculations.

A list of pertinent input data for the parametric analysis is included in Table 1, and a summary of results of the analysis is presented in Fig. 7, which shows the relation between $(RTNDT_s)_c$ and pressure (p) for $RTNDT_o$ = −7°C and for several values of T_f and n, ignoring the beneficial effects of WPS. The dashed lines in Fig. 7 correspond to both incipient initiation (II) and incipient failure (IF), the latter corresponding to no crack arrest following crack initiation. The solid line corresponds to II only; however, as indicated, only a small increase in $RTNDT_s$ is required for failure, except as the pressure approaches zero. As already mentioned, thermal shock alone will not drive the flaw completely through the wall.

Table I. Input data for parametric analysis

Vessel dimensions, mm	
Outside diameter	4800
Inside diameter	4370
Cladding thickness	5.4
Flaw type	Long, axial, through clad
T_i, °C	288
T_f, °C	66, 93, 121, 149
n, min^{-1}	0.015 — ∞
t_{max}, h	2, 1[a]
h_f, $W \cdot m^{-2} \cdot °C^{-1}$	5680, 1700[a]
p, MPa	0—17.2 in 1.72 increments
$RTNDT_o$, °C	−29, −7, 4

[a]Used in a few cases for comparison purposes.

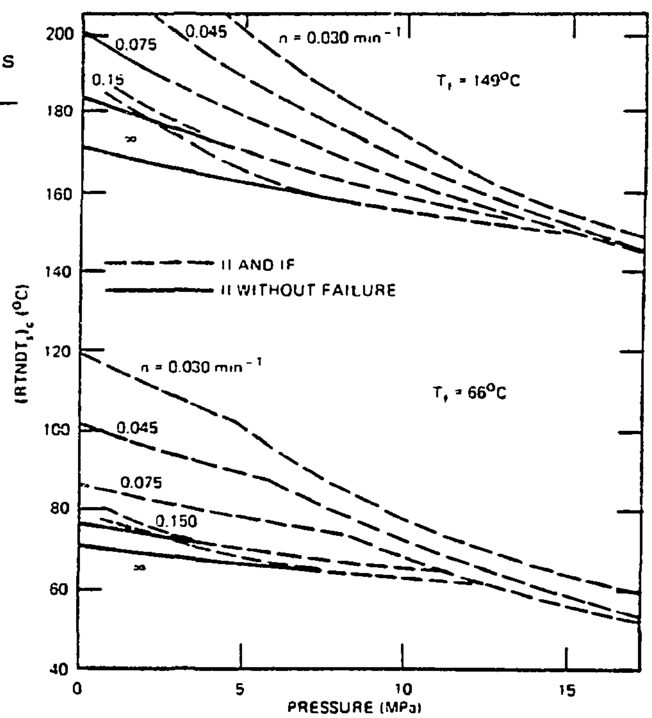

Fig. 7. Summary of results for OCA parametric analysis showing $(RTNDT_s)_c$ vs p for two values of T_f and three values of n and ignoring the beneficial effects of WPS.

The results in Fig. 7 show that at high pressure and for $n>0.030\ min^{-1}$, $(RTNDT_s)_c$ is insensitive to the rate at which the coolant temperature decreases; and for the highest pressure considered (17.2 MPa, which is approximately the safety-valve setting) it was found that over the range of T_f values considered (66–149°C)

$$(RTNDT_s)_c \approx 1.10\ T_f - 22°C \quad . \tag{5}$$

Equation 5 might be used for obtaining a conservative maximum permissible value of $RTNDT_s$ by specifying a reasonable minimum value of T_f. Suppose such a value of T_f is 120°C. Then the maximum permissible value of $RTNDT_s$ would be ~110°C.

The sensitivity of $(RTNDT_s)_c$ to $RTNDT_o$ was found to be rather small (~3°C) over the range of $RTNDT_o$ values considered. Furthermore, the sensitivity to t_{max} over the range of 1 to 2 h and to h_f over the range of 1700 to 5680 $W \cdot m^{-2} \cdot °C^{-1}$ was found to be small except for a few cases involving a sensitivity to t_{max} as shown in Table II. For very slow transients ($n = 0.015\ min^{-1}$), $(RTNDT_s)_c$ decreased significantly with the decrease in t_{max}. Of course for cases where II takes place prior to 1 h (see Figs. 4 and 5), changing t_{max} from 2 to 1 h would make no difference. This tends to be the case for the more rapid transients.

Table II. Effect of h_f and t_{max} on critical values of $\Delta RTNDT_s$ corresponding to II without WPS

Case			$(\Delta RTNDT_s)_c$, °C			
T_f	n	p	h_f, $W \cdot m^{-2} \cdot °C^{-1}$ / t_{max}, hr			
°C	min^{-1}	MPa	5680/2	1700/2	5680/1	1700/1
66	0.015	3.4	152	157	196	208
66	0.015	17.2	101	107	163	173
66	0.15	3.4	79	95	79	95
66	0.15	17.2	58	61	61	71
149	0.015	3.4	>220	>220	>220	>220
149	0.015	17.2	177	181	216	>220
149	0.15	3.4	180	194	180	194
149	0.15	17.2	151	153	151	157

Another sensitivity investigated was that of $(RTNDT_s)_c$ to the imposed limit on the maximum critical crack depth. Decreasing this limit tends to increase $(RTNDT_s)_c$, and the increase is larger for high-pressure cases since the critical crack depths are greater for higher-pressure transients. Calculations were made for two limiting fractional crack depths of 0.15 and 0.076 and for $n = 0.015$ and $0.15\ min^{-1}$, $T_f = 66$ and 149°C, and for $p = 17.2$ MPa. The differences in $(RTNDT_s)_c$ associated with the two limits on critical crack depth were small, the maximum values being 8°C.

ANALYSIS OF SEVERAL RECORDED PWR OCA's

Several PWR OCA's have occurred in recent years, and recordings of the pressure and temperature transients have been used as input to fracture-mechanics analyses, using the FM model described herein. The temperature transients were measured upstream of the injection point for the emergency core coolant and thus do not necessarily reflect the temperature of the coolant in the downcomer. However, in the absence of more accurate data the recorded transients were used so as to obtain some indication of the severity of actual OCA's in terms of pressure vessel integrity.

Table III. Values of $(RTNDT_s)_c$ for several recorded PWR OCA's

Plant	(date)	$(RTNDT_s)_c$ w/o WPS, °C	
		Flaw Orientation	
		Long.	Cir.
Robinson	(1970)	161 (F)[a]	177 (A)
Robinson	(1972)	193 (F)	>249
Robinson	(1975)	179 (F)	189 (A)
Rancho Seco	(1978)	146	165 (A)
TMI-2	(1979)	98 (F)	124 (F)
R. E. Ginna	(1982)	—	192 (F)

[a] A and F in parentheses indicate arrest (with no reinitiation) and failure.

The accidents analyzed and the results obtained are shown in Table III. The values of $(RTNDT_s)_c$ correspond to either incipient initiation followed by crack arrest and no reinitiation or to incipient initiation and failure, as indicated; WPS was ignored, and the imposed limits on critical fractional crack depth were 0.025 and 0.15, the lower limit disallowing crack initiation in the cladding. Because copper and nickel concentrations can be very much different in the circumferential and axial welds, $(RTNDT_s)_c$ was calculated for both crack orientations for the plate-type vessels.

Estimates[14] of $(RTNDT_s)_A$ for all PWR pressure vessels in service today indicate that at this time (September 1982) a few vessels have values approaching 120°C for axial welds and 140°C for circumferential welds. Thus, assuming appropriate flaws to exist in the welds, the analysis indicates that these few unidentified vessels would have a potential for failure today, if the reactor facilities were subjected to a TMI-2-type OCA; however, the Rancho Seco-type transient would not be a threat for several more years.

SUMMARY

A state-of-the-art fracture-mechanics model has been developed that is based on LEFM, includes recent modifications to the radiation-damage trend curves and to the fluence attenuation curve, and is believed to be conservative. The results of an OCA parametric analysis indicate that crack propagation will not take place under the most severe accident conditions if $RTNDT_s < 1.10\ T_f - 22°C$, and it was determined that this relation was not sensitive to $RTNDT_o$, h_f or the assumed duration of the transient over a reasonable range of values.

A fracture-mechanics analysis was also performed for several PWR recorded OCA's, and it was determined, based on preliminary estimates of actual values of $RTNDT_s$ for existing PWR vessels, that a few vessels may have a potential for failure in a few years if subjected to the 1978 Rancho Seco-type transient.

Presumed conservatisms in the fracture-mechanics model are associated with arrest on the upper shelf, the effects of cladding on surface extension of short flaws and warm prestressing. These areas are being investigated to determine the degree of conservatism and to see if the model can be modified to remove excessive conservatism, should it exist.

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REFERENCES

1. W. H. Tuppeny, Jr., W. F. Siddall, Jr., and L. C. Hsu, *Thermal Shock Analysis of Reactor Vessels Due to Emergency Core Cooling System Operation*, Combustion Engineering, Inc., Report A-68-9-1, March 15, 1968.

2. R. C. Hutto, C. D. Morgan, and W. A. Van De Sluys, *Analysis of the Structural Integrity of a Reactor Vessel Subjected to Thermal Shock*, Babcock & Wilcox Power Generation Division, Topical Report BAW10018, May 1969.

3. D. J. Ayres, W. F. Siddall, Jr., *Finite-Element Analysis of Structural Integrity of a Reactor Pressure Vessel During Emergency Core Cooling*, Combustion Engineering Report A-70-19-2, January 1970.

4. (March 20, 1978, Rancho Seco) Nuclear Safety Information Center Accession #0020-138830).

5. (February 26, 1980 Crystal River 3) Nuclear Safety Information Center Accession #0020-160846.

6. (April 23, 1979 Three Mile Island 2) Nuclear Safety Information Center Accession #0020-137918, 137919, 139931.

7. L. E. Steele, *Neutron Irradiation Embrittlement of Reactor Pressure Vessel Steels*, Technical Report Series No. 163, International Atomic Energy Agency, Vienna, 1975.

8. Personal communication with P. N. Randall, U.S. Nuclear Regulatory Commission, 1982.

9. F. J. Loss, A. A. Gray, Jr. and J. R. Hawthorne, *Significance of Warm Prestress to Crack Initiation During Thermal Shock*, Naval Research Laboratory, Washington, DC, NRL/NUREG 8165, September 1977.

10. R. D. Cheverton, et al., "Thermal Shock Investigations," *Heavy-Section Steel Technology Program Quart. Prog. Rep. for October—December 1980*, NUREG/CR-1951 (ORNL/NUREG/TM-437), March 1981, pp. 37—54.

11. R. D. Cheverton et al., "Thermal Shock Investigations," *Heavy-Section Steel Technology Program Quart. Prog. Rep. for October—December 1981*, NUREG/CR-2141, Vol. 4 (ORNL/TM-8252), April 1982, pp. 52—80.

12. S. T. Rolfe and J. M. Barsom, *Fracture and Fatigue Control in Structures, Applications of Fracture Mechanics*, Prentice-Hall, Inc., 1977.

13. T. U. Marston (editor), *Flaw Evaluation Procedures: ASME Section XI*, EPRI NP-719-SR, August 1978.

14. USNRC, "Effects of Residual Elements on Predicted Radiation Damage to Reactor Pressure Vessel Materials," *Reg. Guide 1.99*, Rev. 1 (Sept. 16, 1976).

15. S. K. Iskander, R. D. Cheverton, D. Ĝ. Ball, *OCA-I, A Code for Calculating the Behavior of Flaws on the Inner Surface of a Pressure Vessel Subjected to Temperature and Pressure Transients*, ORNL/NUREG-84, August 1981. OCA-II is a modification of OCA-I. A report is in preparation.

16. R. D. Cheverton, et al., "Thermal Shock Investigations," *Heavy-Section Steel Technology Program Quart. Prog. Rep. for July—September 1981*, NUREG/CR-1197 (ORNL/NUREG/TM-370), April 1980, pp. 52—80.

17. R. D. Cheverton, et al., "Thermal Shock Investigations," *Heavy-Section Steel Technology Program Quart. Prog. Rep. for October—December 1979*, NUREG/CR-1305 (ORNL/NUREG/TM-380), May 1980, pp. 67—70.

18. G. C. Robinson and J. G. Merkle, "Stainless Steel Cladding Investigations," *Heavy-Section Steel Technology Program Quart. Prog. Rep. for October—December 1981*, NUREG/CR-2141, Vol. 4 (ORNL/TM-8252), April 1982, pp. 118—123.