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REVIEWS 3/

FACTORS AFFECTING 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, if certain postulated accidents, referred to as overcooling accidents, were to occur, the pressure vessel could be subjected to severe thermal shock while the pressure is substantial. As a result, 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.

The probability of vessel failure depends upon the probability of the overcooling accident occurring and of the existence of a flaw of appropriate size. Furthermore, because of the radiation-induced reduction in fracture toughness, the probability of vessel failure increases with increasing reactor operating time and copper and nickel concentrations in the vessel material. A fracture-mechanics analysis for a typical postulated accident and also related thermal-shock experiments indicate that very shallow surface flaws that extend through the cladding into the base material could propagate. This is of particular concern because shallow flaws appear to be the most probable and presumably are the most difficult to detect.

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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 an 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 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.[7,8,9] 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 surface 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.

*In Fig. 1 a = depth of flaw, w = wall thickness.

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.

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[10] and demonstrated experimentally[10,11] 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 $\sim$40% of the wall thickness.

Even if WPS were not effective, the flaw could not completely penetrate the wall under thermal-shock loading conditions alone because of the substantial decrease in K_I as the crack tip approaches the outer surface (see Fig. 1). 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, particularly if WPS is ignored. At the present time there is some hesitancy to accept the beneficial effects of WPS in the analysis of postulated OCA's because of uncertainties regarding characteristics of OCA's that might actually take place.

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 effectively infinite in length along 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 on 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 Sect. XI,[13] and the reduction in toughness due to radiation damage was estimated using Eq. (1), which was recently proposed by Randall[9] as a possible revision to Reg. Guide 1.99, Rev. 1.[14]

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

where

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

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

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

RTNDT = $RTNDT_o$ + ΔRTNDT,

$RTNDT_o$ = initial (zero fluence) value of RTNDT,

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

A typical attenuation of the fluence through the wall of the vessel that includes a correction for the effect of the change in neutron spectrum through the wall on radiation damage was also recently proposed by Randall[9] and is being used in the ORNL studies. The relation is

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

where

F = fast neutron fluence at tip of flaw,

F_o = fast neutron fluence at inner surface of vessel,

a = depth of flaw.

For some postulated OCA's, once crack initiation takes place 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 arbitrary upper-shelf toughness value of 220 MPa $\sqrt{m}$ prior to a calculated arrest event.

A procedure used for evaluating the integrity of a pressure vessel is to calculate, using the above model, the threshold or critical values of RTNDT corresponding to incipient initiation of a flaw and incipient failure 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. 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. These values of RTNDT are referred to herein as $(RTNDT_s)_c$, the critical value, and $(RTNDT_s)_A$, the actual value.

To obtain the critical values of RTNDT it is necessary to specify a transient, the fracture-mechanics model, a failure criterion and a value of $RTNDT_o$, 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 exist. For most of the PWR vessels in service today so-called high concentrations of copper are found only in the welds, and presumably flaws are much more likely to exist in the welds than elsewhere. Therefore, the weld zones close to the core, where the fluence is relatively high, are of particular concern.

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} K_{Ia} $= f$ (T, $RTNDT_o$, ΔRTNDT) 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).

$$\Delta RTNDT = \Delta RTNDT_S \, e^{-2.54 \times 10^{-3} a \, mm^{-1}} \quad (3)$$

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.

Figures 3 and 4 contain two crack-initiation curves: one for shallow flaws (reduced to a single point for incipient initiation) and the other for deeper flaws that initiate later in the transient. Initiation of shallow flaws is primarily the result of thermal stresses and the relatively low fracture toughness near the inner surface, while initiation of deep flaws is sensitive to pressure stresses and occurs later in the transient when temperatures are lower. At later times in the life of the vessel the initiation "loops" expand, join up and generally take on the appearance of that shown in Fig. 2, which indicates the ability of very shallow flaws (~5 mm deep) to initiate. Because of this apparent ability, very shallow surface flaws are of concern in the evaluation of vessel integrity during OCA's.

In the event of an OCA, the integrity of the vessel cannot be challenged unless one or more flaws of appropriate size and location exist. Thus, the probability of vessel failure is related to the probability of having an inner-surface flaw of a particular depth in a weld opposite the reactor core. This

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probability at the time the vessel goes into service can be expressed as

$P(a)$ = number of surface cracks in a specific weld with depths in the range Δa about $\underline{a}$ as the vessel goes into service

$$= f(a) \cdot \Delta a \cdot N \cdot V \cdot B(a) \;, \qquad (4)$$

where

$f(a)$ = (fraction of cracks with depths in the range $a \rightarrow a + da$)/da ,

in which case

$$\int_{a=0}^{a=w} f(a)\, da = 1 \;;$$

Δa = a specified range of crack depths about $\underline{a}$ such that

$$\sum_i \Delta a_i = w \;;$$

N = number of cracks of all sizes per unit volume of weld material prior to preservice inspection;

V = volume of specific weld;

$B(a)$ = (number of undetected cracks in Δa about $\underline{a}$ following preservice inspection)/(all cracks in Δa prior to repairs)*,

= (number of cracks in Δa when vessel goes into service)/(total number of cracks in Δa prior to repairs).

Correlations for the crack-size probability distribution function, $f(a)$, and the probability of nondetection, $B(a)$, have been proposed by several investigators,[16,17,18] and those selected for our OCA studies are shown in Fig. 6.

*It is assumed that all detected flaws are repaired.

As indicated, if a flaw exists, it is much more likely to be very shallow than deep, and the shallower a flaw the more difficult it is to detect.

EVALUATION OF THE FRACTURE-MECHANICS MODEL

The validity of LEFM for application to thermal-shock problems has been verified in a series of thermal-shock experiments[11] that were designed to exhibit flaw behavior trends calculated to exist during OCA's. There are still areas of uncertainty, but in most of these areas the FM model described herein is believed to be conservative. The degree of conservatism is not known at this time, but programs are under way to obtain such information. The presumed conservative features in the model include (1) the use of long flaws as opposed to the more probable finite-length flaws; (2) no crack arrest above some arbitrary upper-shelf toughness value as opposed to taking advantage of possibly higher crack arrest toughness and/or resistance to ductile tearing; and (3) to some extent a disregard for the beneficial effects of WPS. A possibly non-conservative feature is the use of the crack-size and crack-detection probability functions in Fig. 6, which tend to constitute averages of rather widely differing functions proposed by several investigators. On the other hand, presumably it is more likely that flaws will reside below the cladding, where they have less tendency to propagate, than extending through it, as assumed in the OCA studies, and the probability functions do not distinguish between these two types of flaws.

ANALYSIS OF SEVERAL POSTULATED OCA'S AND ONE ACTUAL OCA

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[19] was conducted assuming a constant pressure and an exponential decay of the downcomer coolant temperature. The temperature transient is expressed as

$$T_c = T_f + (T_i - T_f)\, e^{-nt} , \tag{5}$$

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 duration of the transient was limited to 2 h, and the fluid-film heat transfer coefficient, which is a necessary input to OCA-II, was assumed to be independent of time and was assigned a value that is achieved with the main circulating pumps running ($5680\ W \cdot m^{-2} \cdot {}^\circ C^{-1}$).

A summary of results of the analysis for long axial flaws 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) (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.

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)

$$(RTNDT_s)_c \simeq 1.10\ T_f - 22^\circ C . \tag{6}$$

Equation (6) 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. For lower values of p and n than those represented by Eq. (5), the permissible values of $RTNDT_s$ would be higher.

In addition to analyzing the above postulated transients, calculations were made for several PWR OCA's that have actually occurred in recent years, and this was done using recordings of the pressure and temperature transients as input to the fracture-mechanics analyses. 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.

One of the OCA's calculated took place at Rancho Seco in 1978, and the recorded temperature and pressure transients, after some smoothing, are shown in Fig. 8. Calculations were made for flaws oriented in both the axial and circumferential directions, and the corresponding values of $(RTNDT_s)_c$ for incipient failure, ignoring WPS, were found to be 146 and 166°C, respectively.

Estimates[9] of $(RTNDT_s)_A$ for all PWR pressure vessels in service today indicate that at this time (April 1983) 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 the Rancho Seco-type transient is not an immediate threat but could be for a few vessels in several more years.

SUMMARY AND CONCLUSIONS

A few PWR's have been subjected to transients that have resulted in thermal shock to the reactor pressure vessel, and there are a number of postulated accidents that could do the same. A state-of-the-art fracture-mechanics model has been developed and used to estimate the extent to which such transients could threaten the integrity of the vessel. It has been determined analytically and demonstrated experimentally that very shallow flaws can propagate as a result of some of these accidents, provided that the vessel has been in service long enough for radiation damage to be substantial.

The results of a parametric study of postulated accidents indicate that crack propagation will not take place under the most severe accident conditions if $RTNDT_s < 1.10\ T_f - 22°C$, where T_f is the final or asymptotic coolant temperature. Calculations were also performed for several PWR transients that have actually occurred, including the 1978 Rancho Seco accident. It was determined, based on preliminary estimates of actual values of $RTNDT_s$ for existing PWR vessels, that there is no immediate threat to the integrity of PWR vessels in the event of Rancho Seco-type accidents; however, in several more years there could be.

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Figure Captions

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

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

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

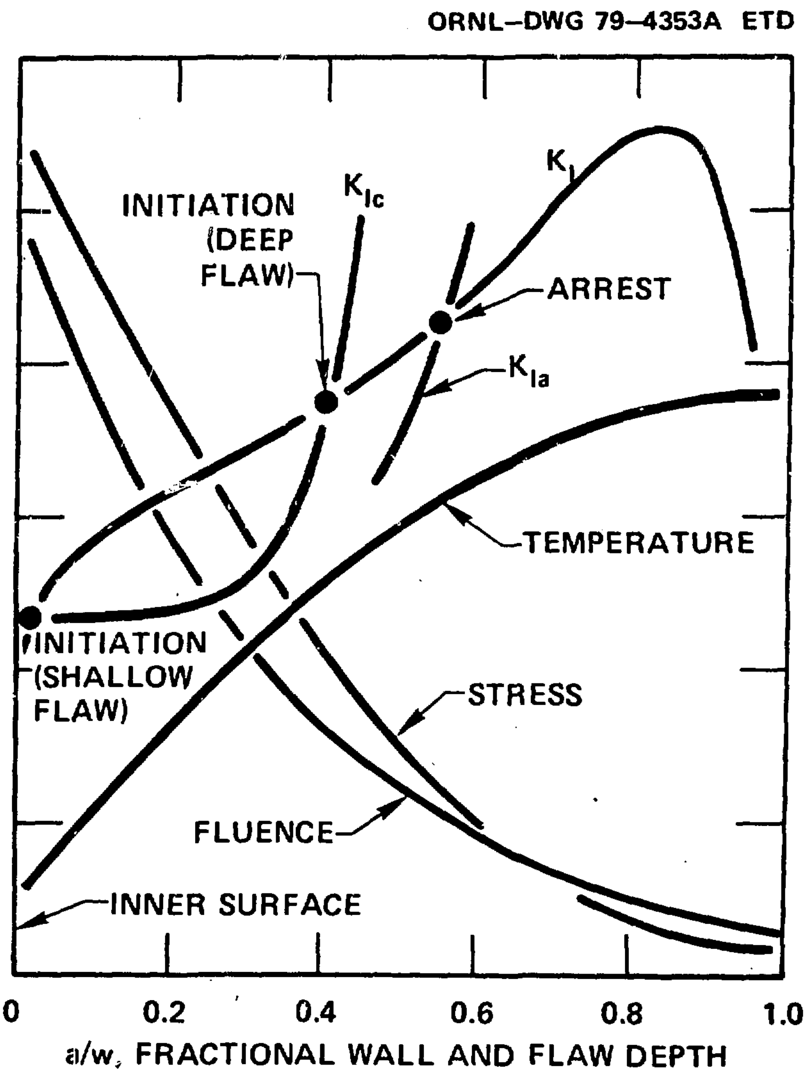
Fig. 4. Critical-crack-depth curves for an OCA illustrating incipient initiation followed by arrest and no reinitiation.

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

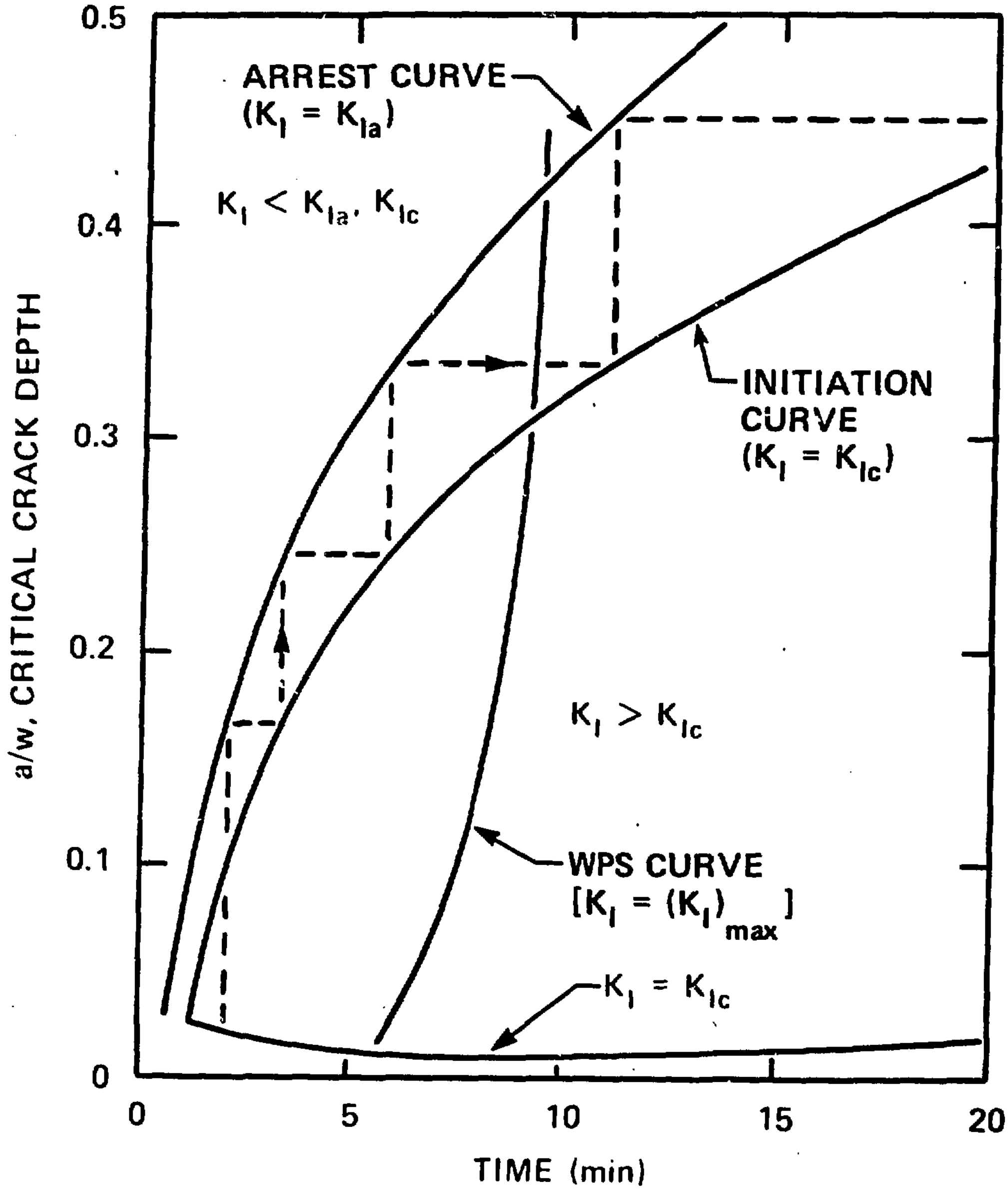
Fig. 6. Crack-size and -nondetection probability functions used in ORNL OCA studies (from Ref. 18).

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

Fig. 8. Rancho Seco 1978 OCA coolant-temperature and pressure transients (smoothed).


ORNL–DWG 79–4353A ETD
K_{Ic}
K_I
INITIATION (DEEP FLAW)
ARREST
K_{Ia}
TEMPERATURE
INITIATION (SHALLOW FLAW)
STRESS
FLUENCE
INNER SURFACE
0
0.2
0.4
0.6
0.8
1.0
a/w, FRACTIONAL WALL AND FLAW DEPTH

ORNL-DWG 80-5312B ETD



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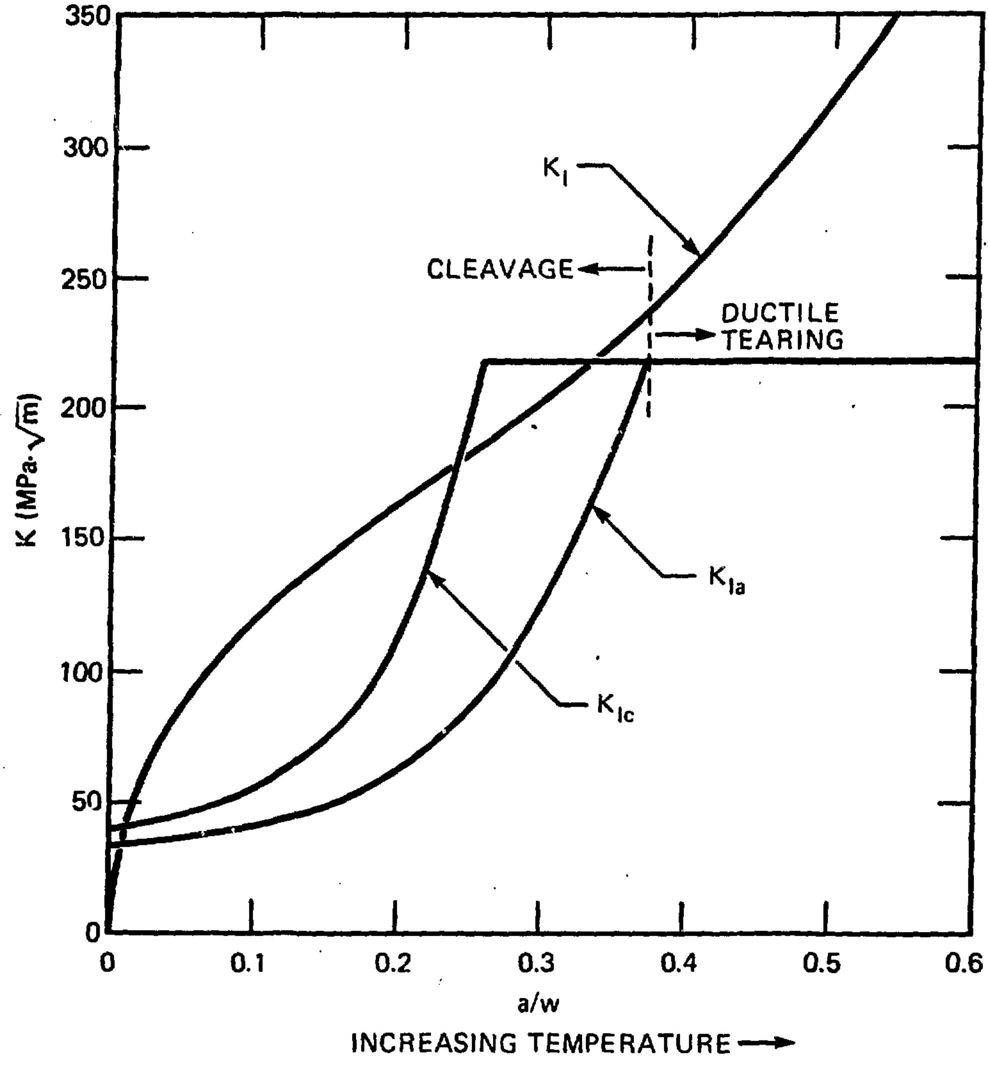
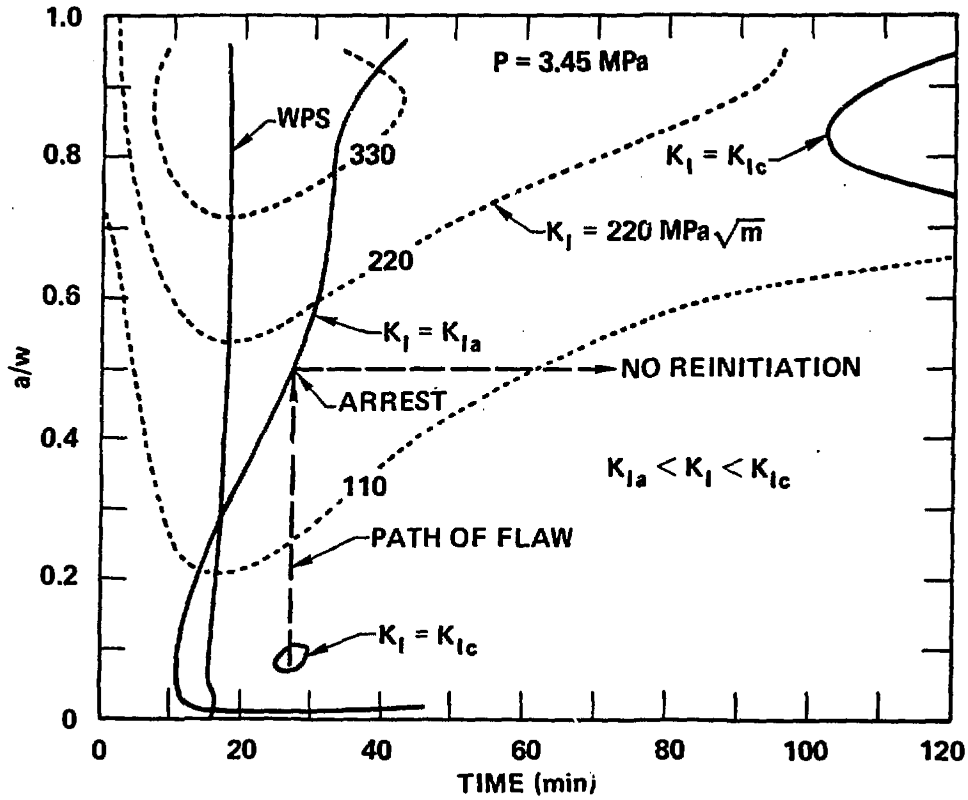
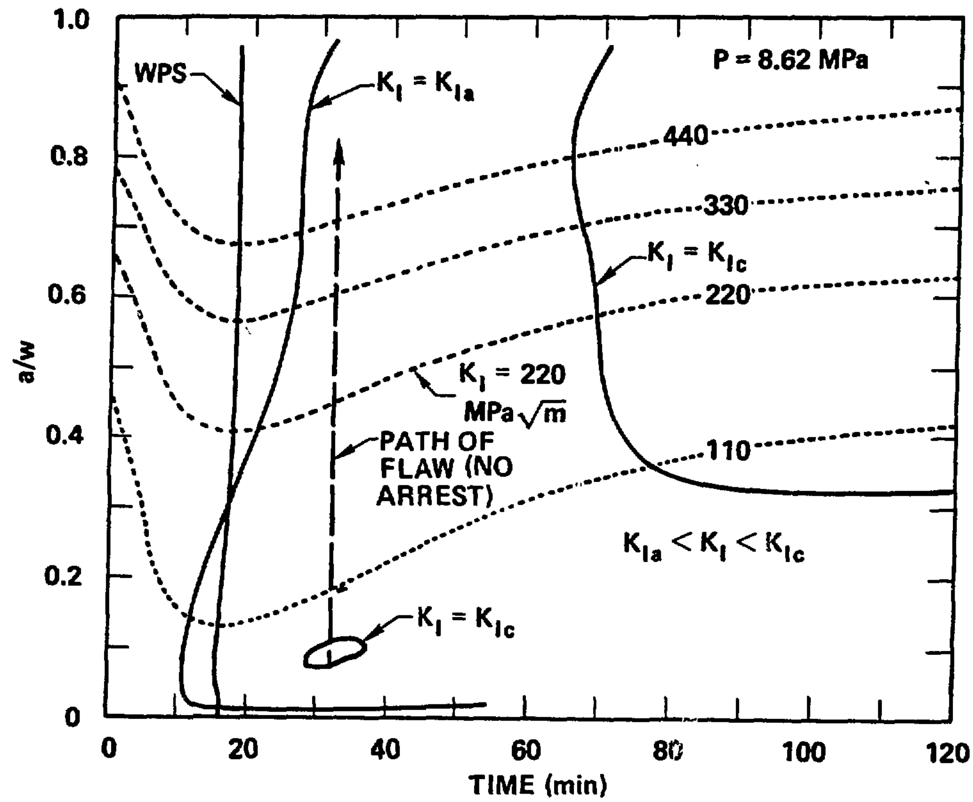


350
300
250
200
150
100
50
0
K (MPa·√m)
K_I
CLEAVAGE
DUCTILE TEARING
K_Ia
K_Ic
0
0.1
0.2
0.3
0.4
0.5
0.6
a/w
INCREASING TEMPERATURE



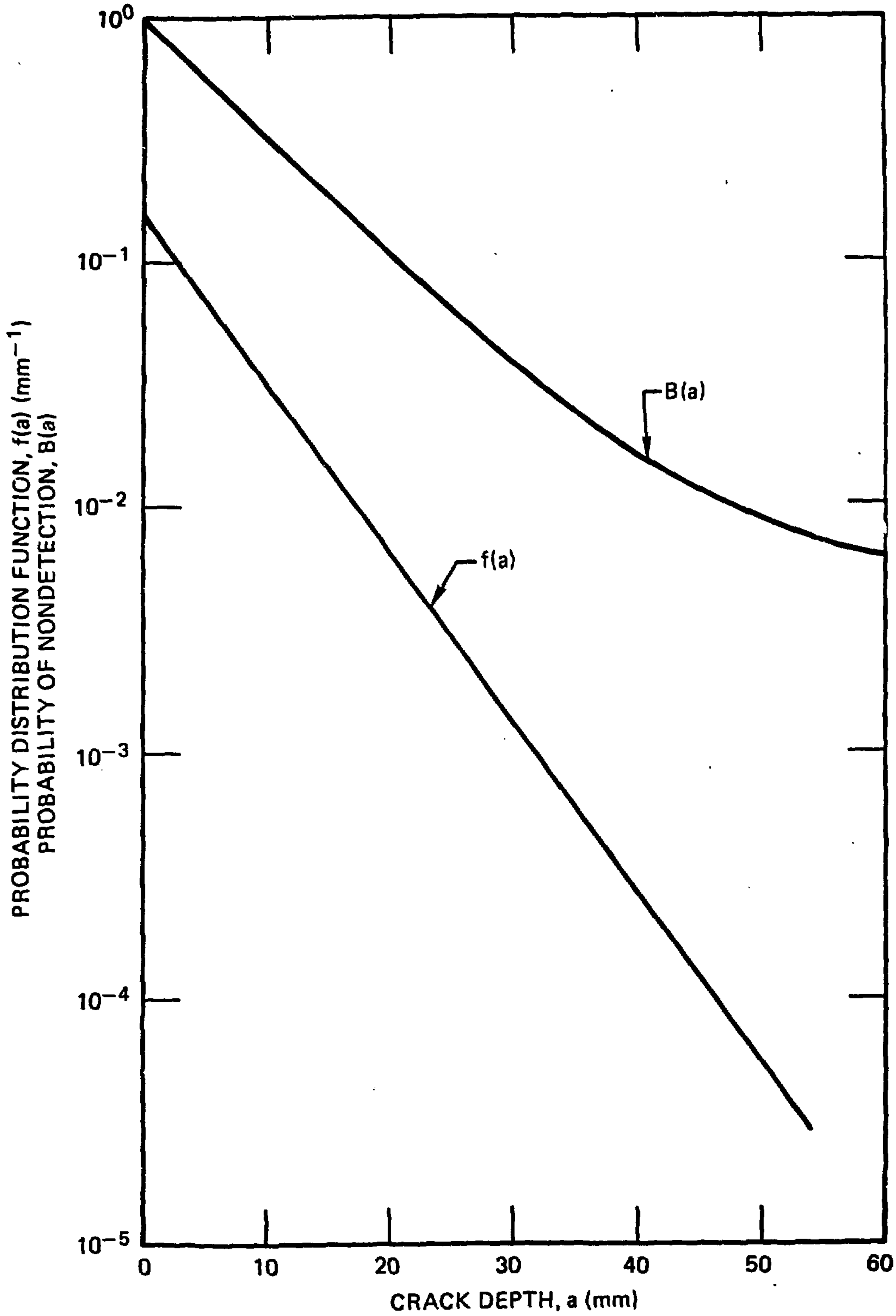
P = 3.45 MPa
WPS
330
220
110
$K_I = K_{Ic}$
$K_I = 220\ MPa\sqrt{m}$
$K_I = K_{Ia}$
ARREST
NO REINITIATION
$K_{Ia} < K_I < K_{Ic}$
PATH OF FLAW
$K_I = K_{Ic}$
a/w
TIME (min)
1.0
0.8
0.6
0.4
0.2
0
0
20
40
60
80
100
120

4



P = 8.62 MPa
WPS
$K_I = K_{Ia}$
440
330
$K_I = K_{Ic}$
220
$K_I = 220$ $MPa\sqrt{m}$
PATH OF FLAW (NO ARREST)
110
$K_{Ia} < K_I < K_{Ic}$
$K_I = K_{Ic}$
a/w
1.0
0.8
0.6
0.4
0.2
0
0
20
40
60
80
100
120
TIME (min)

ORNL–DWG 83–4488 ETD



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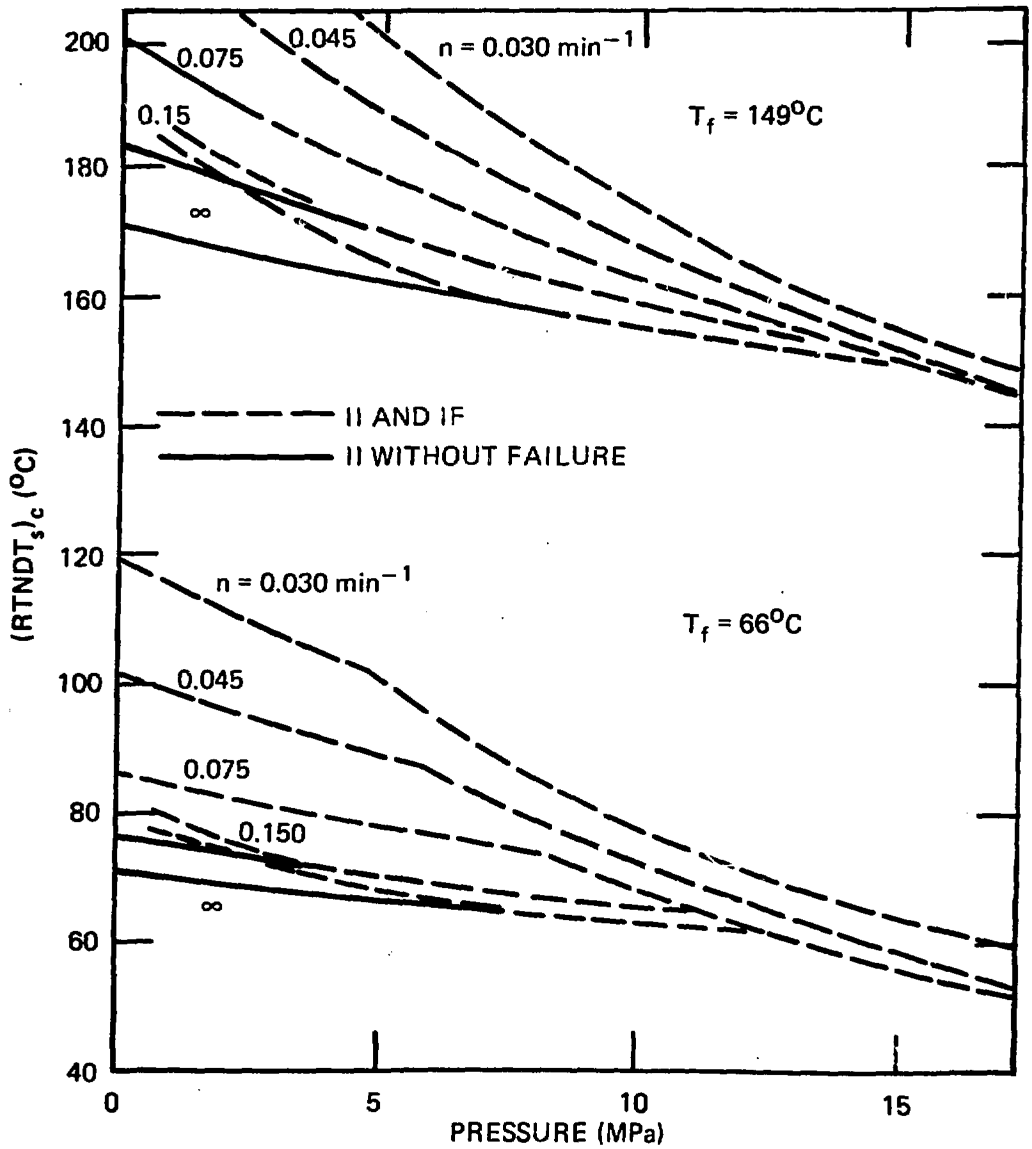

(7)

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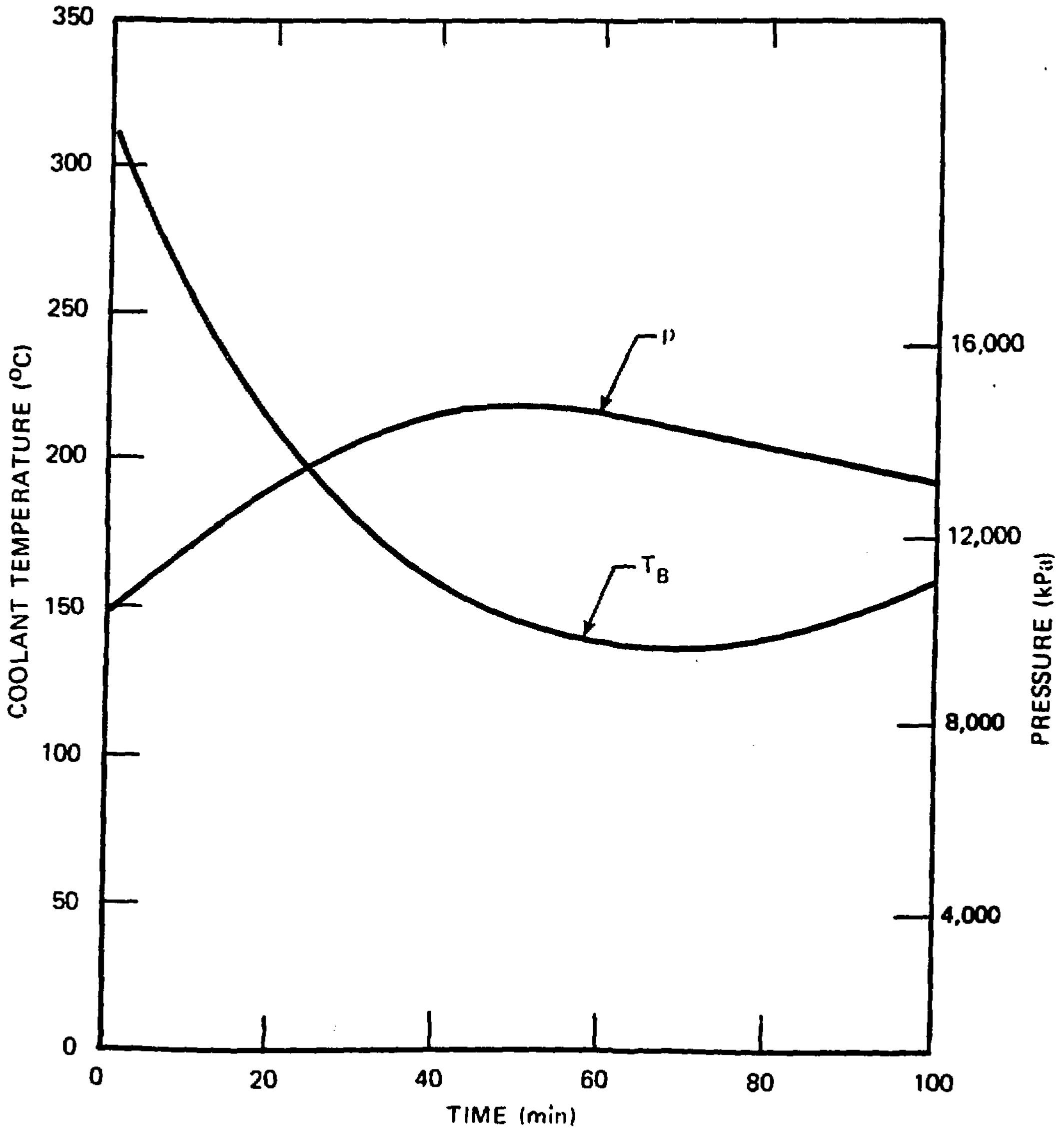

(8)

$5\frac{3}{4}$" (85% reduction)