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NRC STAFF EVALUATION

OF

PRESSURIZED THERMAL SHOCK

(SEPTEMBER 13, 1982 DRAFT)

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1. INTRODUCTION

1.1 Background

Reactor pressure vessels (RPVs) in nuclear power plants have traditionally been considered extremely reliable structural components. Indeed, studies completed in the United States and Europe have concluded that the disruptive failure rate (loss of the pressure retaining boundary) for nuclear pressure vessels is less than 10^{-6} per year at a 99% confidence level for RPVs designed, fabricated, inspected, and operated in accordance with the Boiler and Pressure Vessel Code of the American Society of Mechanical Engineers. However, recent results from surveillance and research programs and operating experience suggest that the issue of RPV failure probability should be reassessed. The renewed interest in RPV failure probability is due to the observation that thermal hydraulic transients occurring in commercially operating nuclear power plants are subjecting RPVs to unanticipated loadings which could contribute significantly to the failure probability of the RPV. In addition, operating experience and research programs over the past few years have provided additional information that more clearly defines both material property variations in RPVs and the effect of neutron irradiation on the material's resistance to fracture.

As a result of operating experience, it is now recognized that transients can occur in pressurized water reactors (PWRs) characterized by severe overcooling causing thermal shock to the vessel, concurrent with or followed by repressurization (that is, pressurized thermal shock, PTS). In these PTS transients, rapid cooling of the reactor vessel internal surface causes a temperature distribution across the reactor vessel wall. This temperature distribution results in thermal stress with a maximum tensile stress at the inside surface of the vessel. The magnitude of the thermal stress depends on the temperature difference across the reactor vessel wall. The effects of this thermal stress are compounded by pressure stresses if the vessel is pressurized.

Severe reactor system overcooling events which could be accompanied by pressurization or repressurization of the reactor vessel (PTS events) can result from a variety of causes. These include instrumentation and control system malfunctions, and postulated accidents such as small break loss-of-coolant accidents (LOCAs), main steam line breaks (MSLBs), feedwater pipe breaks, or stuck open valves in either the primary or secondary system. As long as the fracture resistance of the reactor vessel material remains relatively high, such events are not expected to cause failure. After the fracture toughness of the vessel is reduced by neutron irradiation (and this occurs at a faster rate in vessels fabricated of materials which are relatively sensitive to neutron irradiation damage), severe PTS events could cause propagation of fairly small flaws that are conservatively postulated to exist near the inner surface. The assumed initial flaw might initiate and propagate into a crack through the vessel wall of sufficient extent to threaten vessel integrity and, therefore, core cooling capability.

The PTS issue is a concern only for operating PWRs. Boiling water reactors (BWRs) do not have a significant PTS concern. BWRs operate with a large portion of water inventory inside the pressure vessel at saturated conditions. Any sudden cooling will condense steam and result in a pressure decrease, so simultaneous creation of high pressure and low temperature is improbable. Also contributing to the lack of PTS concerns for BWRs is the lower fluence at the vessel inner wall, and the use of a thinner vessel wall which results in a lower stress intensity for a postulated crack.

1.2 Staff Reviews of PTS Information Provided by Licensees and Industry

Evaluations of Pressurized Thermal Shock by the NRC staff in the spring of 1981 concluded that no immediate licensing actions were required at that time, but that since the vulnerability of reactor vessels to overcooling events increases as the vessels accumulate additional neutron irradiation, extensive further investigations were needed to determine whether and when corrective actions will be needed to provide assurance of vessel integrity throughout the intended service life of nuclear plants.

On March 31, 1981, the NRC staff held the first of many meetings that were to occur over the following sixteen months with licensees, reactor manufacturers, and owners groups to discuss pressurized thermal shock concerns and exchange technical information.

Subsequently, the NRC, in letters dated August 25, 1981, requested the licensees of eight plants representative of older reactor vessels to provide more detailed information on the present and projected pressure vessel materials properties, on the probability and possible severity of events that could cause failure of embrittled vessels, and on the efficacy and feasibility of several potential corrective actions.

Many of the event-sequence analyses provided by licensees in response to the August 25, 1981 letter can be characterized as design-basis event analyses of the type generally submitted in Safety Analysis Reports in support of license applications. Such analyses tend not to be of much help in evaluations of PTS. Many of the assumptions in such analyses were developed and accepted for licensing purposes without regard to PTS concerns. While they appear to be appropriately conservative for calculations of reactor core thermal performance, PTS evaluations need best estimate calculations of pressure and temperature behavior. In addition, some potential event sequences that are not generally analyzed in detail in Safety Analysis Reports, because their consequences are bounded by the design-basis event analyses, can be of greater significance for PTS evaluations. Thus, it is clear that plant-specific PTS evaluations must include a systematic examination of many potential events, with particular attention to the probability and consequences of various possible operator actions and omissions, and equipment malfunctions.

Appendix A to this report summarizes the meetings that have been held with industry, licensee responses to the August 25 letters, and the NRC staff audits of operating procedures, operator qualifications, and training with respect to the PTS issue. Appendix B lists significant events and meetings concerning PTS. Appendix C is a more detailed discussion of the procedures and training audits.

As a result of the review of the extensive information provided by the industry, and of studies and analyses performed by the staff, assisted by contractors and consultants (see particularly the fracture mechanics calculations performed by Oak Ridge National Laboratory described in Appendix O, and the report of a technical review of PTS issues performed by Pacific Northwest Laboratory, Reference 1.1), in the spring of 1982, the staff reaffirmed its previous assessment that no immediate plant modifications were needed to protect against PTS events (other tha nnrovements in procedures and operator training already underway). However, the staff concludes that some plants will require hardware and procedural modifications in the near future. The experience of the past 18 months in generic evaluations of the PTS concerns has made it clear that decisions on the need for, nature of, and timing of, such modifications will require plant-specific, rather than generic evaluations.

1.3 Proposed Approach for Future Evaluations

For the reasons noted above, there is a need for a disciplined technical basis to select plants for which detailed evaluations are required and to determine the timing of such evaluations. The approach proposed by the staff is to select a screening criterion that characterizes the present or projected state of embrittlement of reactor vessels as a function of neutron fluence. Licensees of plants with vessels that are projected to reach the screening criterion within three calendar years would be required to submit detailed, plant-specific evaluations of: the vessel condition; the expected frequency, course and consequences of experienced and postulated overcooling events; plant procedures and operator training related to prevention or mitigation of PTS events; possible modifications of plant equipment, systems and procedures that could reduce the probability and/or severity of overcooling events; possible improvements in in-service inspection methods that could provide increased assurance of the detection of existing flaws in critical regions of the pressure vessel; and possible modifications to decrease the rate of vessel embrittlement or actions to recover ductility.

These licensees would also be required to provide a technical basis for the acceptability of continued operation of the plant for the remainder of its design life taking into account the risk of pressure vessel failure from pressurized thermal shock events, based on the above plant-specific evaluations and such remedial actions as are proposed.

The screening criterion proposed by the staff is based on a parameter that characterizes the state of embrittlement of the reactor vessel. This parameter is the reference temperature for nil ductility transition, RT_{NDT}.

RT_{NDT} is a measure of the temperature at which the vessel or weld material begins a transition from a ductile to a "brittle" fracture mode. Its initial value is determined by a destructive testing procedure. As the material is subjected to neutron irradiation the value increases. Equations have been developed to calculate the shift in RT_{NDT} as a function of neutron fluence for various chemical compositions of the material based on measurements of irradiated materials. The value of RT_{NDT} at a given time can be used in fracture mechanics calculations to determine whether assumed pre-existing flaws would propagate as cracks when the vessel is subjected to overcooling events.

The staff's approach to selection of an RT_{NDT} screening criterion has been to consider the overcooling events that have occurred in U.S. PWRs and, using a deterministic fracture mechanics algorithm, calculate the value of RT_{NDT} for which assumed pre-existing flaws in the reactor vessel would be predicted to extend (grow deeper into the vessel wall). These "critical" values of RT_{NDT} were related to the expected frequency of the initiating events, based on the limited data base (only eight events in 350 reactor-years), and a value of RT_{NDT} was selected for use as the screening criterion.

In addition, the staff considered a wide spectrum of postulated overcooling events that have not occurred. These events were grouped into types, estimates were made of their expected frequency, and stylized characterizations of the temperature and pressure time-histories were developed for each event type. A probabilistic treatment of the fracture mechanics calculations was developed that permitted performance of studies to gain insights into the sensitivity of the fracture mechanics calculations to uncertainties in the various input parameters. By combining the calculated frequencies and characteractics of postulated events with the probabilistic fracture mechanics results, some very approximate estimates of the probability of vessel failure resulting from PTS events were developed and used by the staff to provide some insight into the residual risks inherent in use of the screening criterion approach for further evaluations and resolution of the issue of pressurized thermal shock.

1.4 Structure of this Report

This report provides the NRC staff's technical basis for the selection of the screening criterion, and a brief description of the type of plant-specific analyses that would be required for plants with pressure vessels that are projected to exceed the criterion.

Section 2 of the report discusses the frequency and characterization of overcooling events that have actually been experienced. Section 3 summarizes deterministic fracture mechanics calculations performed for these experienced events and parametric studies of crack growth potential as a function of the event characteristics and RT_{NDT} values. Section 4 combines the results of Sections 2 and 3 and proposes values of RT_{NDT} for use as a screening criterion.

Section 5 presents the staff's proposed method for estimation of vessel-specific values of RT_{NDT} for comparison with the screening criterion.

Section 6 describes an evaluation of the frequency and character of potential lower probability overcooling events.

Section 7 summarizes sensitivity studies performed using a probabilistic treatment of the fracture mechanics calculations that can be used in combination with the results of Section 6 to estimate probabilities of vessel failure. Consideration of these results is presented in Section 8.

Section 9 indicates the nature and timing of the plant-specific evaluations that would be requested for plants approaching the screening criterion.

Section 10 presents the conclusions and recommendations of the NRC staff regarding near-term actions and future programs for resolution of the pressurized thermal shock issue.

Reference

1.1 L. T. Pedersen, W. J. Apley, S. H. Bian, L. J. Defferding, M. H. Morgenstern, P. J. Pelto, E. P. Simonen, D. L. Stevens and T. T. Taylor, Pacific Northwest Laboratory "PNL Technical Review of Pressurized Thermal Shock Issues," USNRC Report, NUREG/CR-2837, July 1982

2. FREQUENCY AND CHARACTERIZATION OF EXPERIENCED OVERCOOLING EVENTS

2.1 Introduction

This section of the report describes the staff's review of eight actual overcooling transients of interest as potential PTS initiators. The event descriptions were reviewed and plots of pressure and temperature as functions of time were developed, based on plant data available. These actual pressure and time histories were used as described in Section 3.0 in deterministic fracture mechanics calculations for each event.

In addition, the actual temperature versus time data for each event were fit to a simple stylized characterization of the temperature transients that could be used conveniently in parametric fracture mechanics studies. For this purpose, the fluid temperature at the reactor pressure vessel inner surface is assumed to decrease exponentially from the initial temperature. The equation used is:

$$T = T_o - (T_o - T_f)\ (1 - \exp(-\beta t))$$

Where T_o = initial temperature, °F
T_f = final temperature, °F
β = cooldown parameter, min^{-1}
t = time, min

For each of the operating experience events, the actual event sequences were reviewed and values of T_f and β were selected to characterize the event. The selection of T_f and β required some engineering judgment. In general, the final temperature is selected to characterize the observed value when a temperature plateau is reached that exists for 30 minutes or longer (the thermal time constant of the vessel wall). The cooldown rate is either the "natural" (for example, best fit) cooldown rate or an adjusted value for cases where the temperature increases following termination of the uncontrolled cooldown. The adjusted cooldown rate used is based on the Westinghouse approach, which considered the fracture mechanics response to the actual temperature transients and the fracture mechanics response to the stylized formulation with an adjusted β value. The adjusted β^* is obtained from

$$\beta^* = 2/t^*$$

where: t^* = the time of lowest temperature,
and
β^* is never less than the "natural" cooldown rate, β.

A representative constant value of the pressure was also selected for each event. These stylized representations of the experienced events were then used for comparison with parametric studies of fracture mechanics calculations as described in Section 3.

Finally, the eight events of interest were used to construct a cumulative frequency distribution of observed events as a function of T_f which is considered in Section 4 in selecting a screening criterion.

2.2 Event Descriptions

2.2.1 H. B. Robinson Steam Line Break (04/28/70)

On April 28, 1970, during hot functional testing (no fuel loaded), one of the steam generator safety valve connections failed due to overloading. A 360° circumferential break allowed the safety valve to blow off the main steam line. The plant conditions were:

- 533°F, 2225 psi primary
- 900 psi secondary
- 3 RCPs running
- 45 gpm charging/letdown
- no feedwater to the steam generator

As a result of the 6-in. schedule 80 pipe break, and with no decay heat, the plant cooled down 213°F in 1 hour to a 320°F cold leg temperature. The operator immediately tripped the RCPs (30 seconds) and started the remaining two coolant charging pumps (70 seconds). The minimum primary system pressure was 1880 psi; with the safety injection (SI) setpoint at 1715 psi, no safety injection occurred. The plant was recovered to a normal no-load condition of 2050 psig and charging/letdown reestablished prior to shutdown.

A post-event review of the data indicated that the pressurizer surge line did not empty. A base case analysis was performed for the event. In addition, a sensitivity analysis was performed without RCP trip, with only one charging pump, and with a primary heat source. The analysis showed that the pressurizer would drain and the primary system pressure would fall below the SI setpoint in about 3 minutes. The cooldown was less and the pressures were lower than the base case analysis. It is expected that the operator actions, based on current procedures, would be similar to this sensitivity analysis. The safety valve stand-off piping was redesigned to prevent any similar occurrences.

The transient data for this event are provided in Figure 2-1. For the stylized characterization of the event the staff selected T_f = 295°F, β = 0.08 min^{-1} and pressure of 2000 psig. This exponential temperature curve is compared with the broken loop cold leg temperature data in Figure 2-2.

2.2.2 H. B. Robinson Stuck Steam Generator Relief Valve (11/05/72)

While at nominal full power operating conditions, the operator was using steam generator relief valves to provide RCS temperature control. One valve would not reclose, resulting in the equivalent of a small steam line break. The secondary side blowdown resulted in a reactor trip and safety injection. The overall cooldown rate was 200°F over a 3-hour period, to 340°F during the course of the event. Insufficient information is currently available to address operator actions taken during this event.

The transient data for this event are provided in Figure 2-3. For the stylized characterization of the event the staff selected T_f = 340°F, β = 0.015 min^{-1} and a pressure of 1000 psig. The exponential temperature curve is compared with the cold leg temperature data in Figure 2-4.

2.2.3 H. B. Robinson RCP Seal SBLOCA (05/01/75)

During full power operation, RCP "C" seal number one leakage exceeded the technical specification limit of 6 gpm. A load reduction was commenced at a rate of 10% per minute to 36% power and pump "C" was deenergized. Reactor trip occurred due to a turbine trip resulting from the load reduction. The decision was made to restart pump "C" when seal injection could not be restored to pumps "A" and "B." Shortly after restarting the pump, while at 1700 psig and 480°F, seals number two and three failed on pump "C" and the pressurizer level began to decrease.

Safety injection pumps were manually started, charging flow was diverted to the auxiliary pressurizer spray to reduce pressure and the SI accumulators partially injected when the pressure dropped to 500 psig.

The cooldown for this event was from 450°F to approximately 310°F in one-half hour, with the pressure decreasing from 1700 psig to about 1150 psig over the period of interest. The use of the auxiliary pressurizer spray rapidly reduced the pressure to 500 psig.

The operator used SI to stabilize pressurizer level and pressure while using the main condenser to cool down the plant for RHR entry.

There is no indication that SI was used to repressurize the plant.

The transient data for this event are provided in Figure 2-5. For the stylized characterization of this event, the staff selected T_f = 250°F, β = 0.02 min^{-1} and a pressure of 500 psig. The exponential temperature curve is compared with the broken loop cold leg temperature data in Figure 2-6.

2.2.4 Rancho Seco NNI/ICS (03/20/78) (excess feedwater transient)

On March 20, 1978, the Rancho Seco plant RCS was cooled from 582°F to about 285°F in slightly more than one hour (approximately 300°F/hr), while RCS pressure was about 2000 psig. The transient was initiated by an inadvertent short in a DC power supply causing a loss of power to the plant's non-nuclear instrumentation (NNI). Loss of NNI power caused the loss of most control room instrumentation and the generation of erroneous signals to the plant's Integrated Control System (ICS). The ICS reduced main feedwater, causing the reactor to trip on high pressure. The cooldown was initiated when feedwater was readmitted to one steam generator by the ICS (auxiliary feedwater was restored). The cooldown caused system pressure to drop to the setpoint (1600 psig) for the safety features actuation system, which started the high pressure injection pumps and auxiliary feedwater to both steam generators. High pressure injection flow restored pressure to 2000 psig. With control room instrumentation either unavailable or suspect for one hour and ten minutes (until NNI power was restored), operators continued auxiliary feedwater and main feedwater to the steam generators while maintaining RCS pressure with the high pressure injection pumps.

The transient data for this event are provided in Figure 2-7. For the stylized characterization of this event the staff selected T_f = 285°F, β = 0.10 min^{-1} and a pressure of 2300 psig. The exponential temperature curve is compared with the cold leg temperature data in Figure 2-8.

2.2.5 Three-Mile Island 2 (03/28/79)

This accident was initiated by a loss of normal feedwater to the steam generators resulting in a turbine trip. As a result of the loss of heat sink, the RCS overpressurized and the PORV opened, which is a normal response and in accordance with the NSSS design. The PORV stuck open and remained open for about 2.4 hours, unnoticed by the operator. HPI was actuated on low pressure. However, at about 3 minutes into the event an operator bypassed the injection actuation signal. One HPI was turned off, and the remaining flow was reduced as a result of a high-level indication in the pressurizer. HPI was automatically actuated again at about 3.3 hours into the event. For the first 73 minutes the RCPs were running. After this time the pumps were turned off due to excessive vibration.

The transient data for this event are provided in Figure 2-9. For the stylized characterization of this event, the staff selected T_f = 225°F, β = 0.04 min^{-1} and a pressure of 2300 psig. The exponential temperature curve is compared with the cold leg temperature data in Figure 2-10.

2.2.6 R. E. Ginna SGTR + PORV (01/25/82)

The plant was operating at 100% power with normal pressure and temperature prior to the steam generator tube rupture (SGTR). The SGTR resulted in automatic reactor trip and automatic actuation of safety injection. On the SI signal, automatic containment isolation occurred and the charging pumps were tripped. Both RCPs were tripped by the operator in accordance with plant procedures. The operators attempted to equalize the primary and faulted SG pressure, in accordance with plant procedures, by opening the PORV. The PORV failed open, and the operator manually closed the block valve to stop the coolant loss.

The transient data for this event are provided in Figure 2-11. For the stylized characterization of this event, the staff selected T_f = 325°F, β = 0.12 min^{-1}, and a pressure of 1400 psig. The exponential temperature curve is compared with the cold leg temperature data in Figure 2-12.

The sudden temperature dip at about 45 minutes has been shown not to be significant in the fracture mechanics analysis, and has been ignored in characterizing this event.

2.2.7 Crystal River 3 NNI/ICS (02/26/80) (small-break LOCA transient)

On February 26, 1980, the Crystal River 3 plant experienced a small-break LOCA transient when a power-operated relief valve (PORV) was opened inadvertently. The resulting transient caused a decrease in RCS temperature (whose magnitude is discussed below) with a system pressure of about 2400 psig. The transient was initiated when an electrical short in a DC power supply for the plant's NNI caused a pressurizer PORV to open, a loss of most control room instrumentation, and the generation of erroneous signals to the plant's ICS. The ICS caused a reduction in feedwater flow and a withdrawal of control rods. RCS pressure initially increased, tripping the reactor on high pressure, and then decreased as coolant discharged through the open PORV. The high pressure injection pumps started at 1500 psig and repressurized the RCS to about 2400 psig. The PORV block valve was closed, but flow out of the RCS continued through the pressurizer safety valves. After approximately 30 minutes, the high pressure injection pumps were throttled back, but RCS pressure was maintained at about 2300 psig for the next one and a half hours while shutdown to cold shutdown conditions by normal operating procedures was initiated.

Since temperatures in the downcomer are not measured, and since many of the temperature measurements normally available were lost when instrumentation power was lost, minimum temperatures were calculated.

For the purpose of this evaluation, the minimum downcomer temperature is based on calculated mixing in the downcomer of the HPI with the minimum vent valve flow (1 vent valve), using the TRAC code and Creare (Ref. 2.1) data for thermal mixing. The mean mixed value for T_f is approximately 250°F (the same value indicated by B&W). A cooldown rate of 0.10 is used, based on a preliminary review of the TRAC analysis, and an approximate time span of 20 minutes prior to the operator regaining control of the transient. For the stylized characterization of this event, the staff selected T_f = 250°F, β = 0.10 min^{-1}, and pressure 2300 psig.

2.2.8 Prairie Island SGTR (10/02/79)

This event was similar to the Ginna SGTR; however, the minimum temperature was 350°F with a β of 0.1 per minute. β is estimated from the adjusted β^* value for a cooldown period of approximately 20 minutes. A pressure of 1000 psig was selected. No plots of temperature and pressure data were available.

2.3 Summary of Operating Experience

In addition to the eight events described in Section 2.2, 24 other events which could have led to PTS concern have been identified. The data sources are the work performed by Phung (Ref. 2.2) and the various licensee submittals on PTS. The final temperatures for each of the events are summarized in Figure 2-13. It is noted that the CE submittals did not identify the Millstone-2 and St. Lucie-1 events as PTS events of concern. It is also noted that 2 of the 3 San Onofre-1 events were not identified by Phung. By vendor there are 21 Westinghouse events, 4 CE events, and 7 B&W events. Only the eight events discussed above which resulted in final temperatures of 350°F and less are of interest for the PTS analysis. These are underlined in Figure 2-13. The values of T_f, β and pressure that have been selected to characterize these events are summarized in Table 2-1.

The eight events characterized in Table 2-1 above occurred during approximately 330 reactor-years of PWR operating experience. On that basis, a cumulative frequency distribution has been plotted as a function of the final temperature of the event, T_f, as shown in Figure 2-14.

2.4 Comparison with Westinghouse Characterization of Operating Experience

Westinghouse believes the operational events referred to in this section that occurred in Westinghouse-designed plants should be characterized somewhat differently. (Their most recent discussion is contained in Appendix G.)

The comparison is as follows:

Event	NRC T_f	W T_f
HBR '75	250	327
Ginna	325	300
HBR '70	295	295
HBR '72	340	400
Prairie Island	350	390

The differences are due to three causes, according to Westinghouse.

First, they state that we plotted the cumulative distribution of events incorrectly. We agree, with respect to a much earlier curve we used. We now plot T_f correctly in Figure 2-14.

Table 2-1

Parameters for Stylized Representation of Experienced Events

Event	T_f (°F)	(M^{-1})	P(psig)
Robinson SLB (W) ('70)	295	0.08	2000
Robinson Stuck SG Valve (W) ('72)	340	0.015	< 1000
Robinson RCP Seal SBLOCA (W) ('75)	250	0.02	< 500
Rancho Seco (B&W)	285	0.10	2300
TMI-2 (B&W)	225	0.04	2300
Ginna SGTR (W)	325	0.12	1400
Crystal River-3 (B&W)	250	0.10	2300
Prairie Island SGTR (W)	350	0.10	1000

Second, they state that one of the events (HBR-'70) was a pre-fuel loading event that occurred during testing conducted to detect weaknesses exactly like the one that was found, and, therefore, should not be included. We agree that inclusion of the event is somewhat on the conservative side, but note that deletion of the event would make no significant difference in our conclusion.

Third, they state that we should terminate an event for PTS consideration when the operator gets the plant within Appendix G cooldown limits. We do not agree. Certainly it is true that a shutdown under normal conditions within Appendix G limits is not a PTS concern. However, a cooldown (whether deliberate or uncontrolled) within Appendix G limits immediately following a more rapid cooldown of PTS concern can very well exacerbate the PTS concern and must be considered.

References:

2.1. "Fluid and Thermal Mixing in a Model Cold Leg and Downcomer With Vent Valve Flow," Creare Incorporated, EPRI Report NP-2227, March 1982.

2.2. Phung, D. L., "Pressure Vessel Thermal Shock at U.S. Pressurized Water Reactors: Events and Precursors, 1963 to Mid-1981," ORNL, Interim Report, May 1982.

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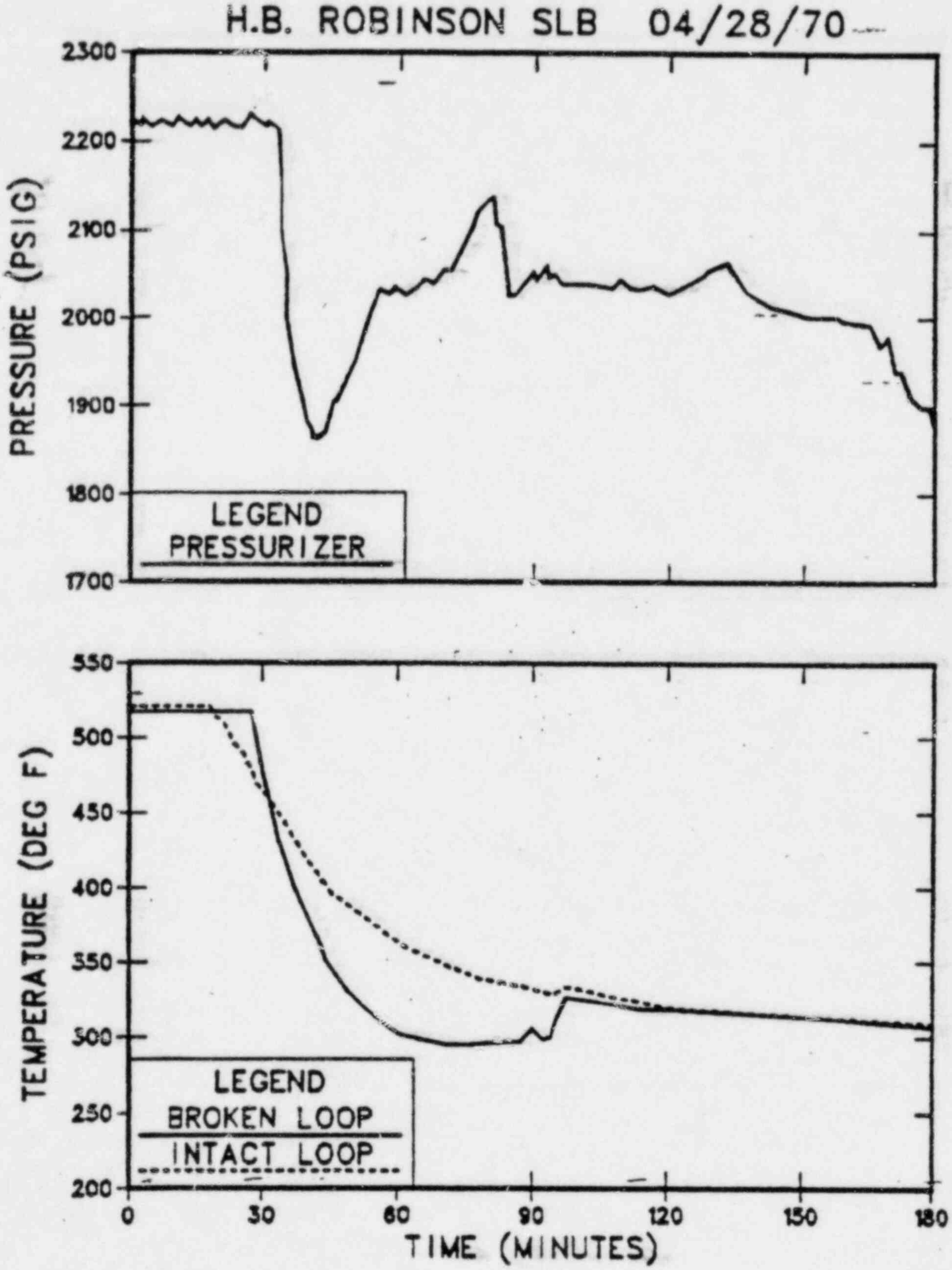

FIGURE 2-1

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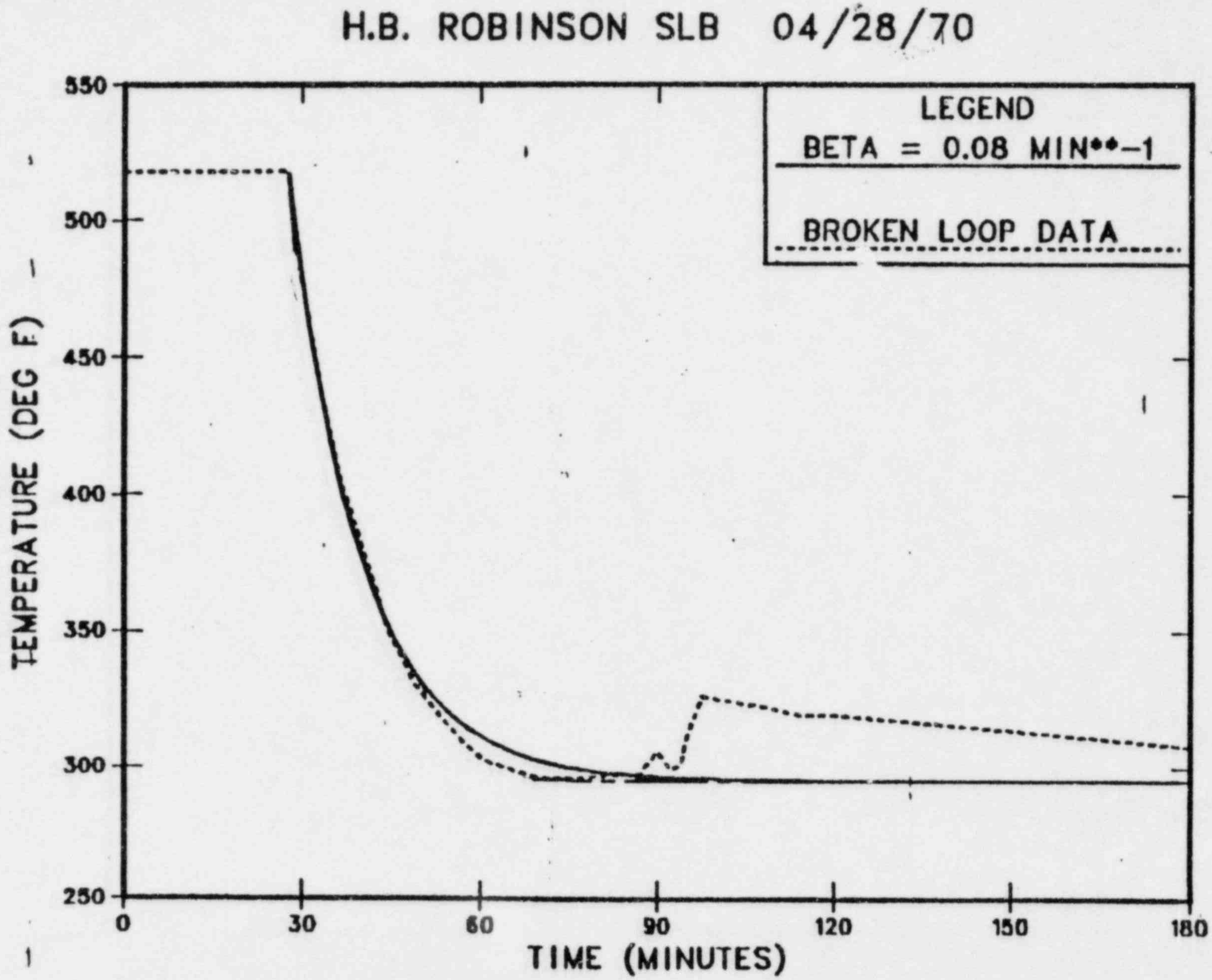

FIGURE 2-2

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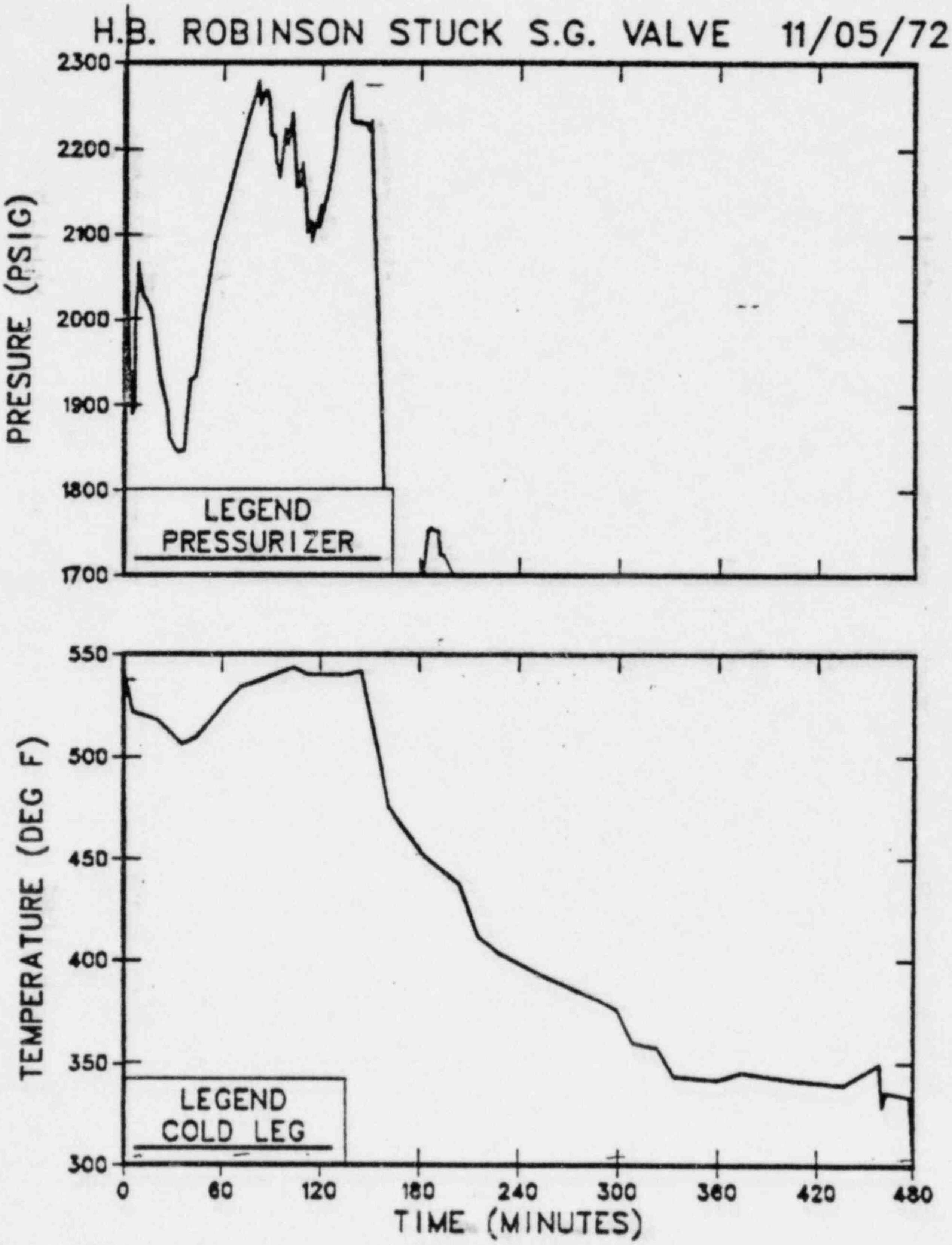

FIGURE 2-3

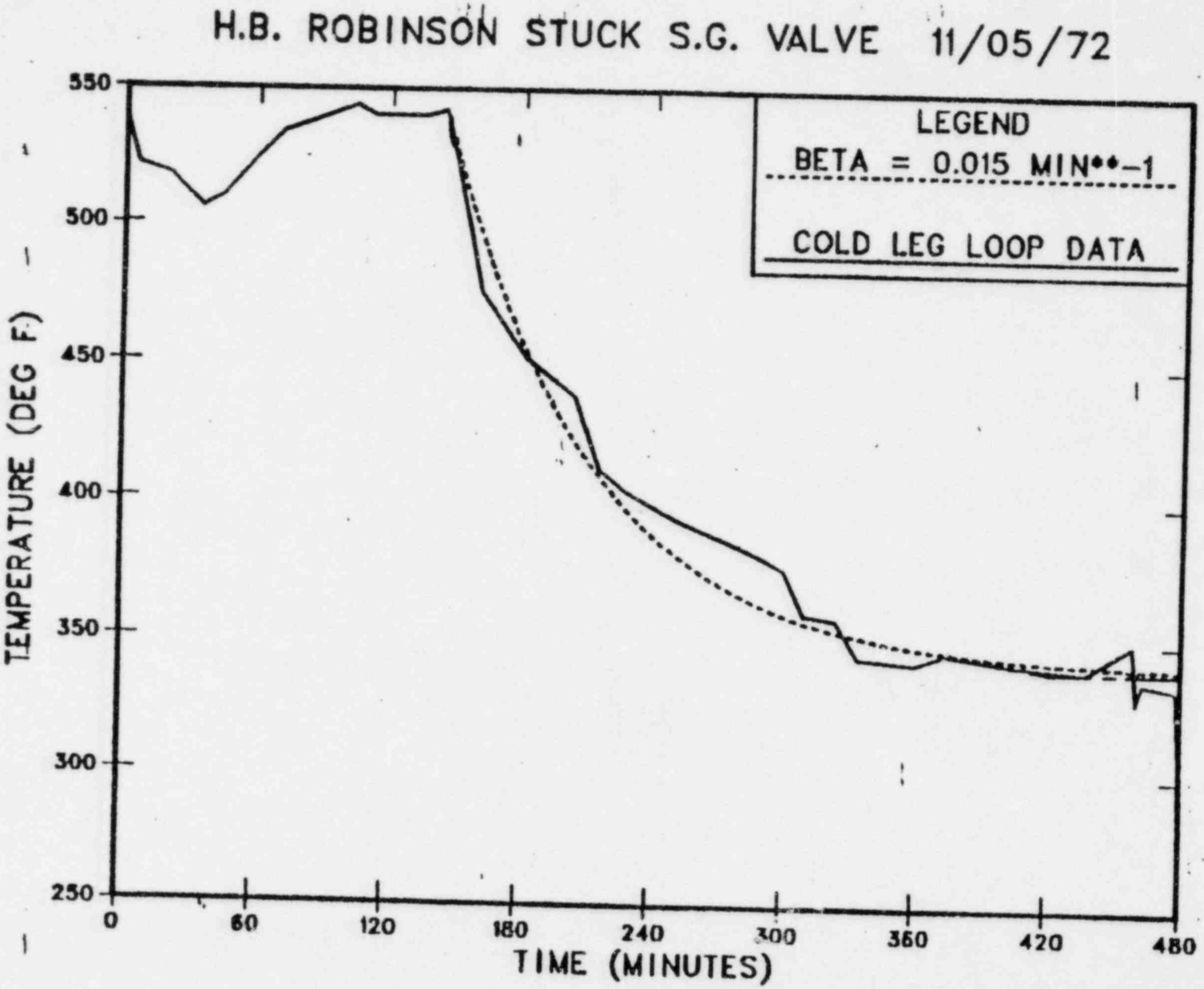

FIGURE 2-4

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H.B. ROBINSON RCP SEAL SBLOCA 05/01/75

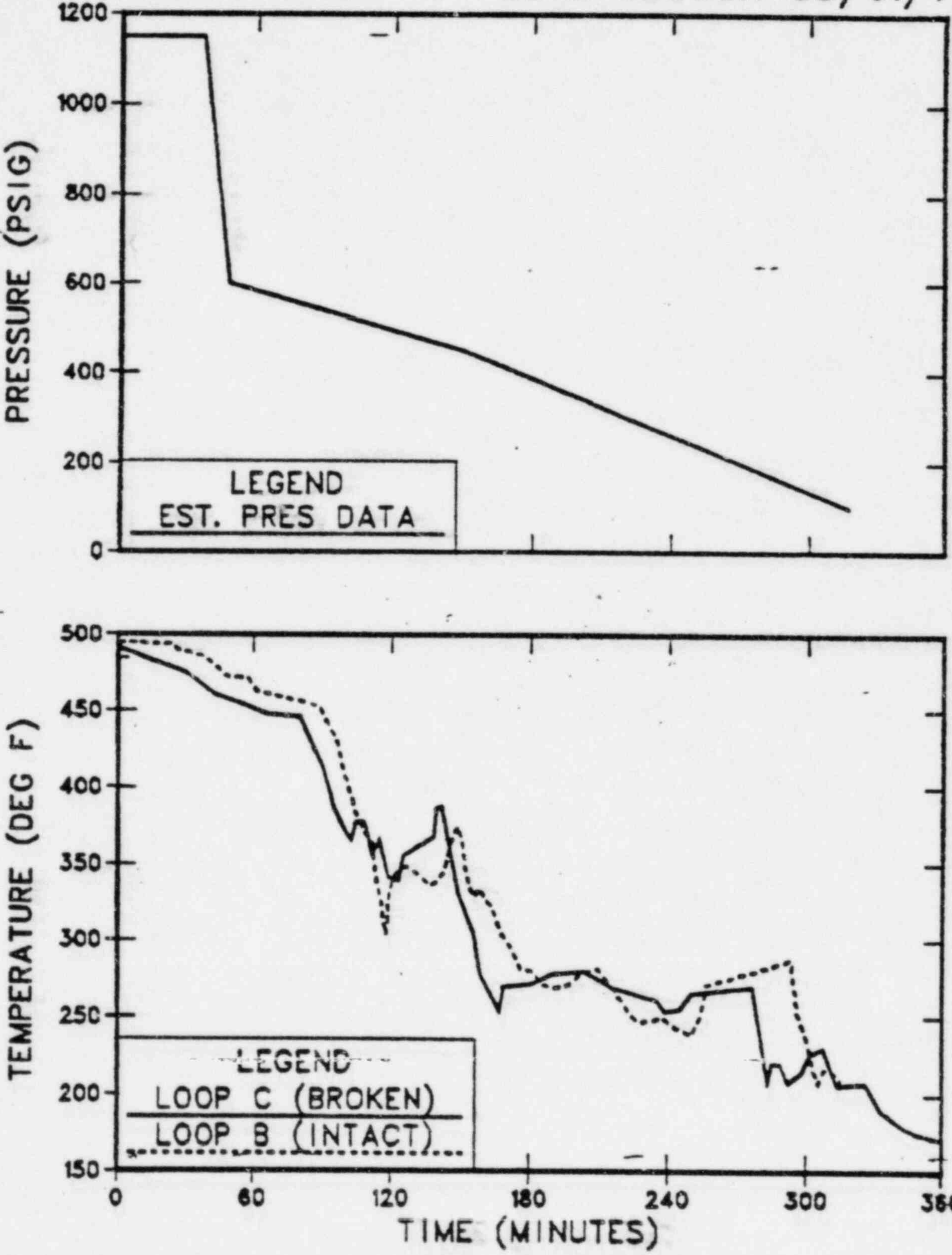

FIGURE 2 - 5

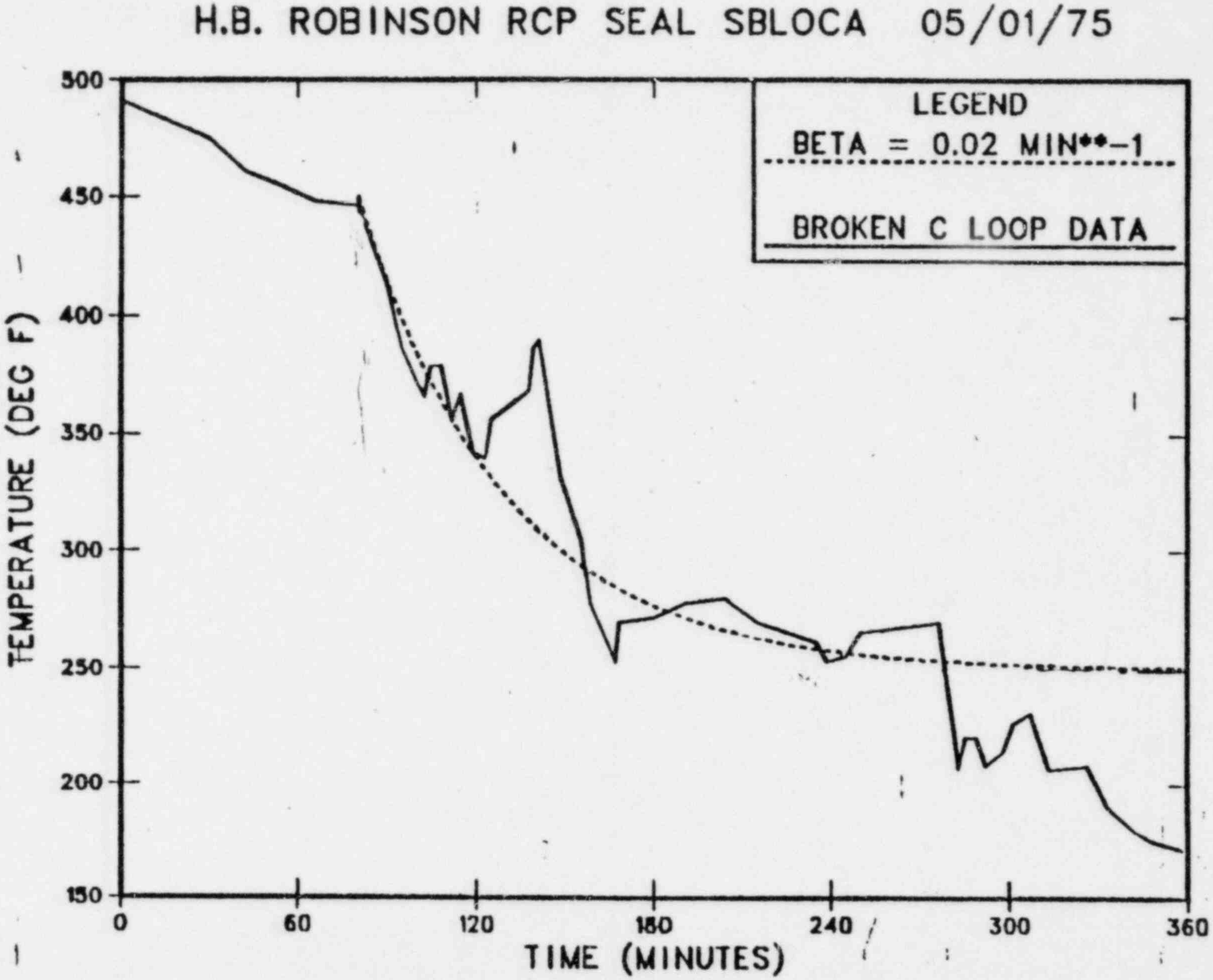

FIGURE 2-6

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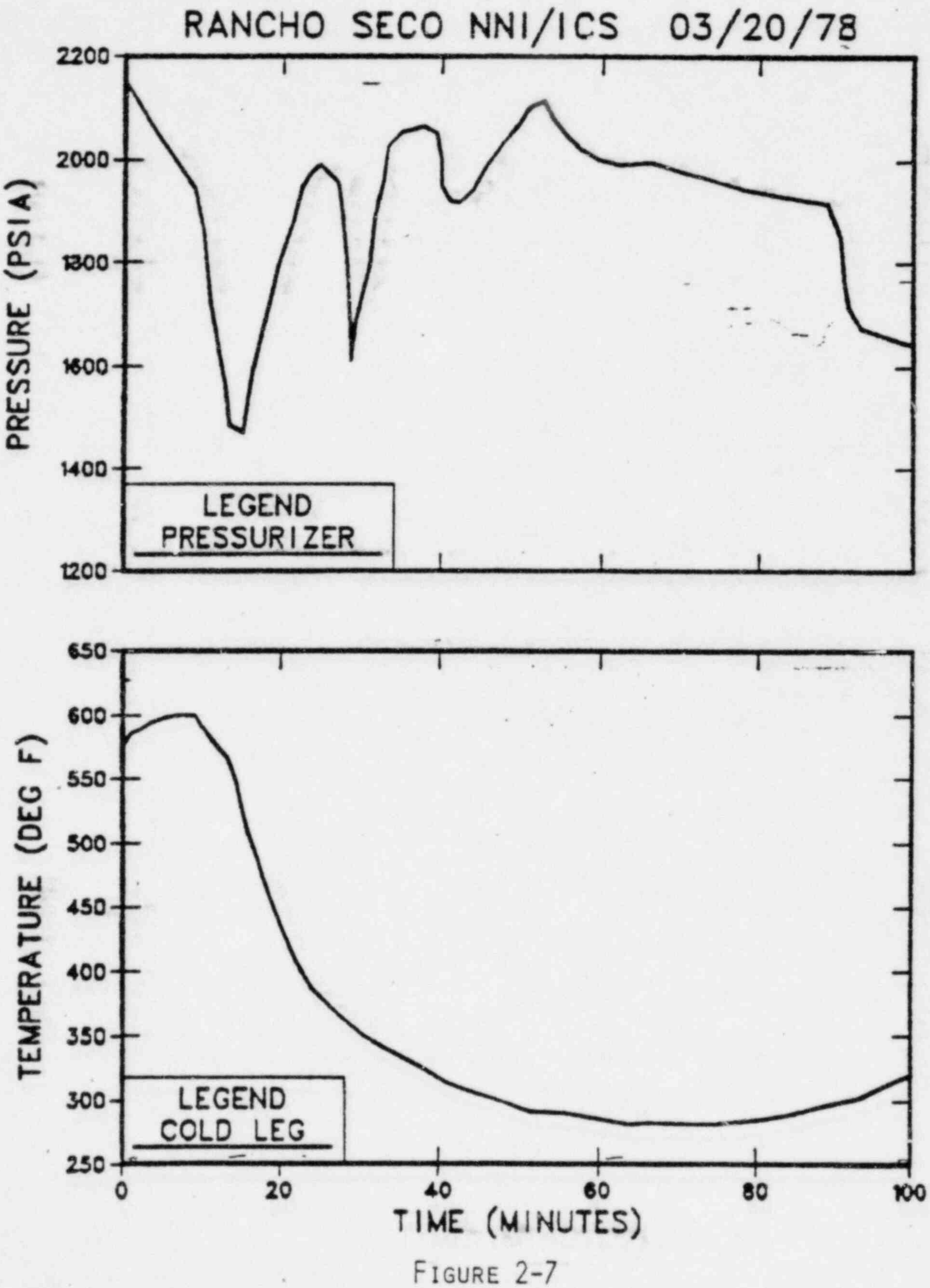

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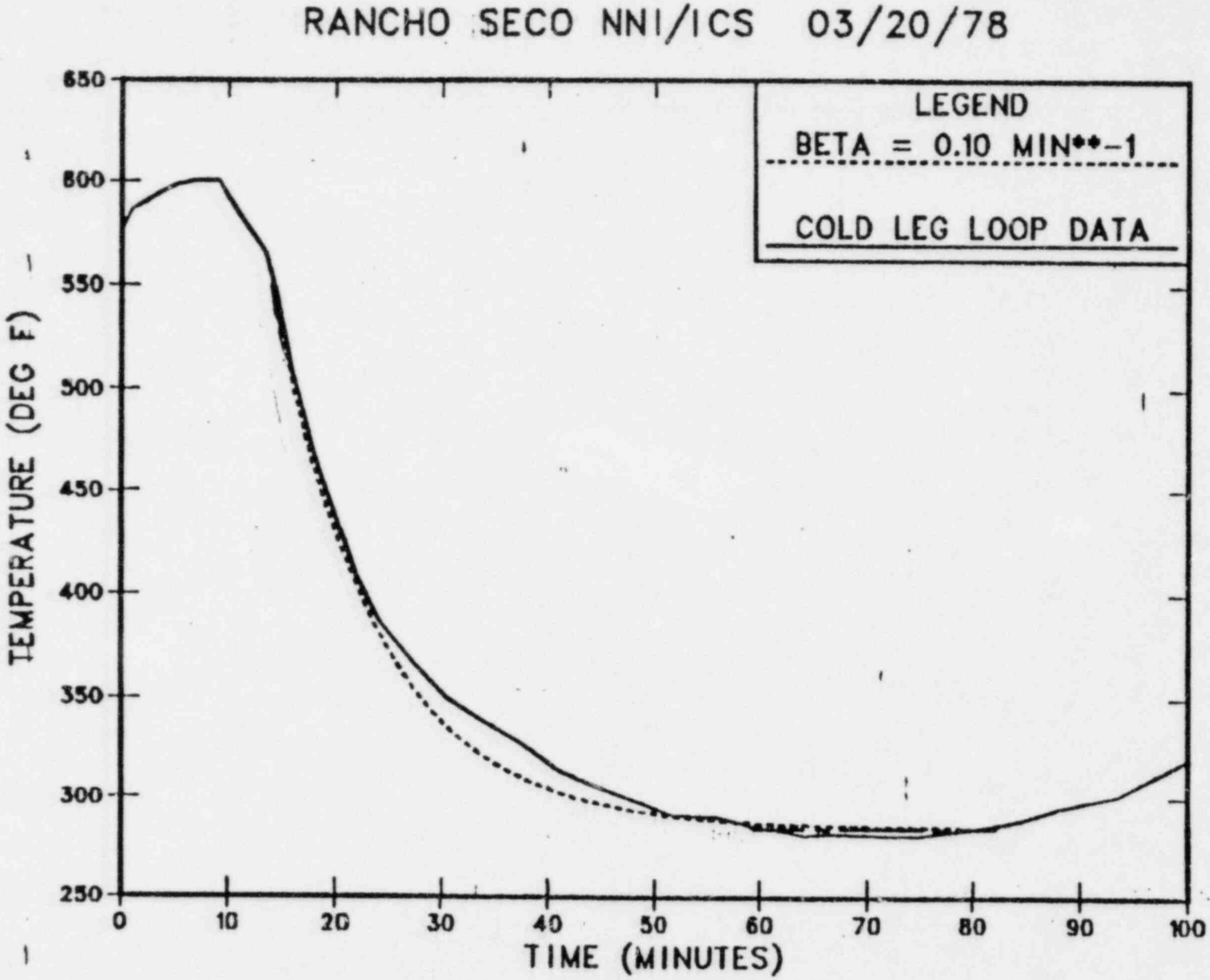

FIGURE 2-8

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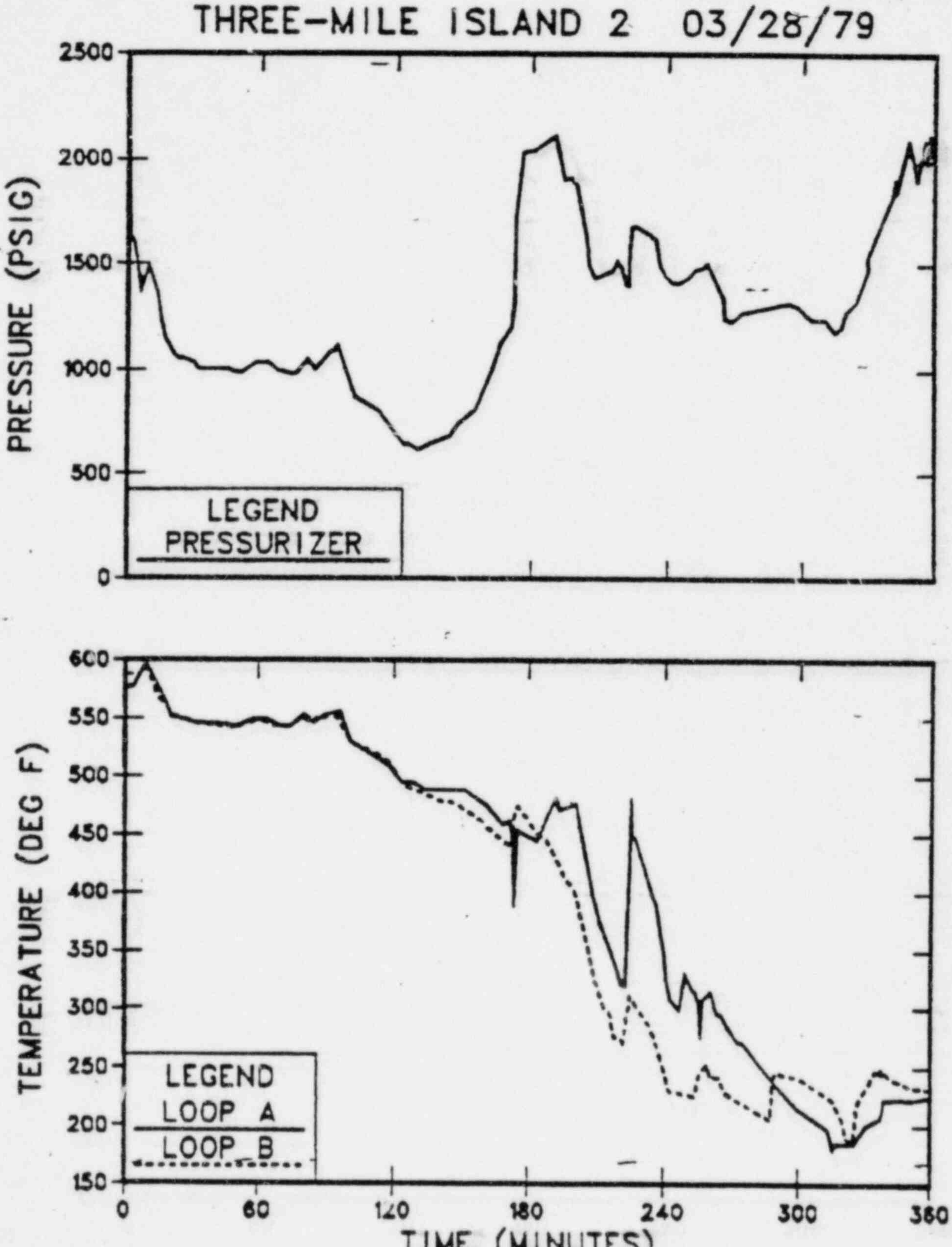

FIGURE 2-9

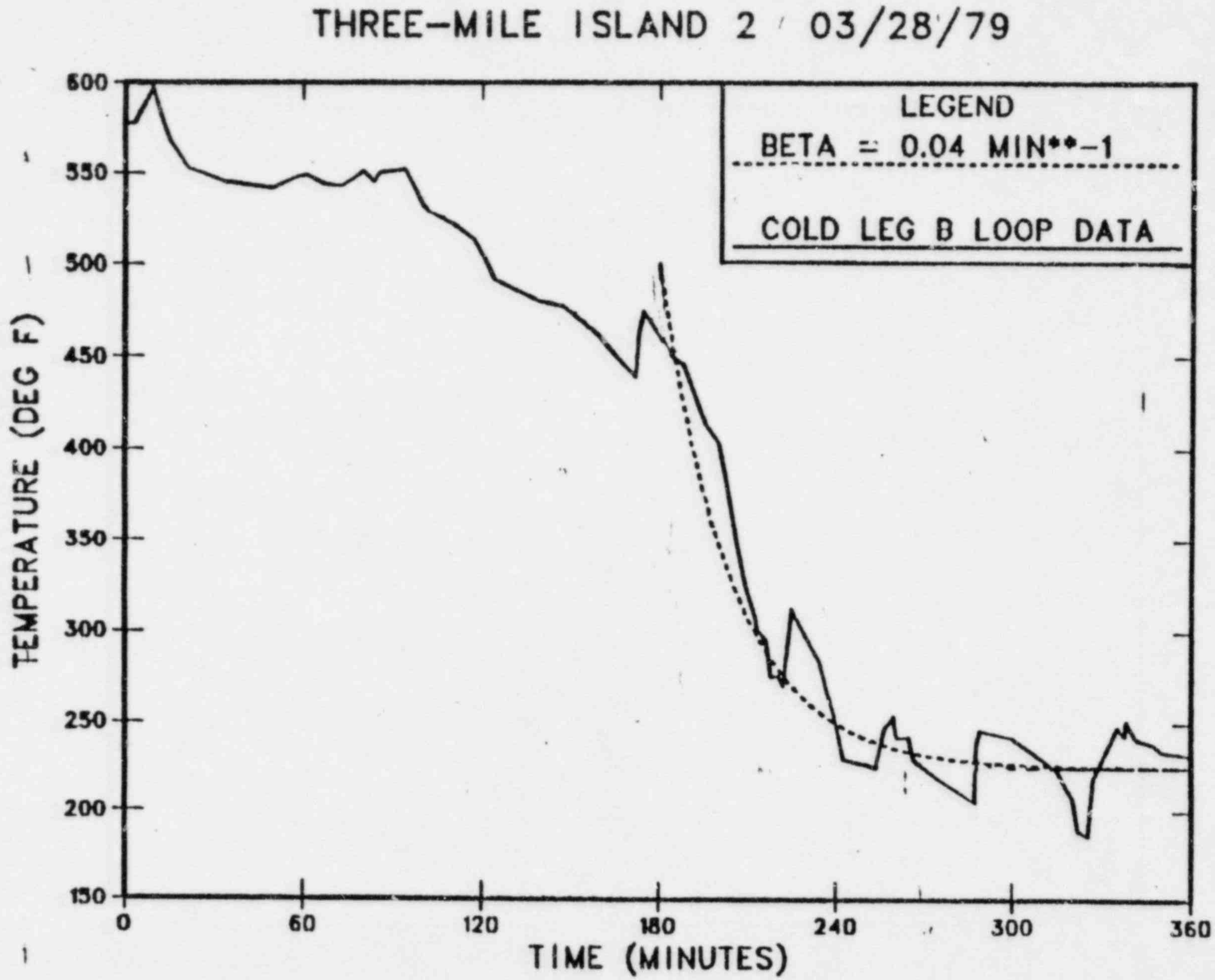

FIGURE 2-10

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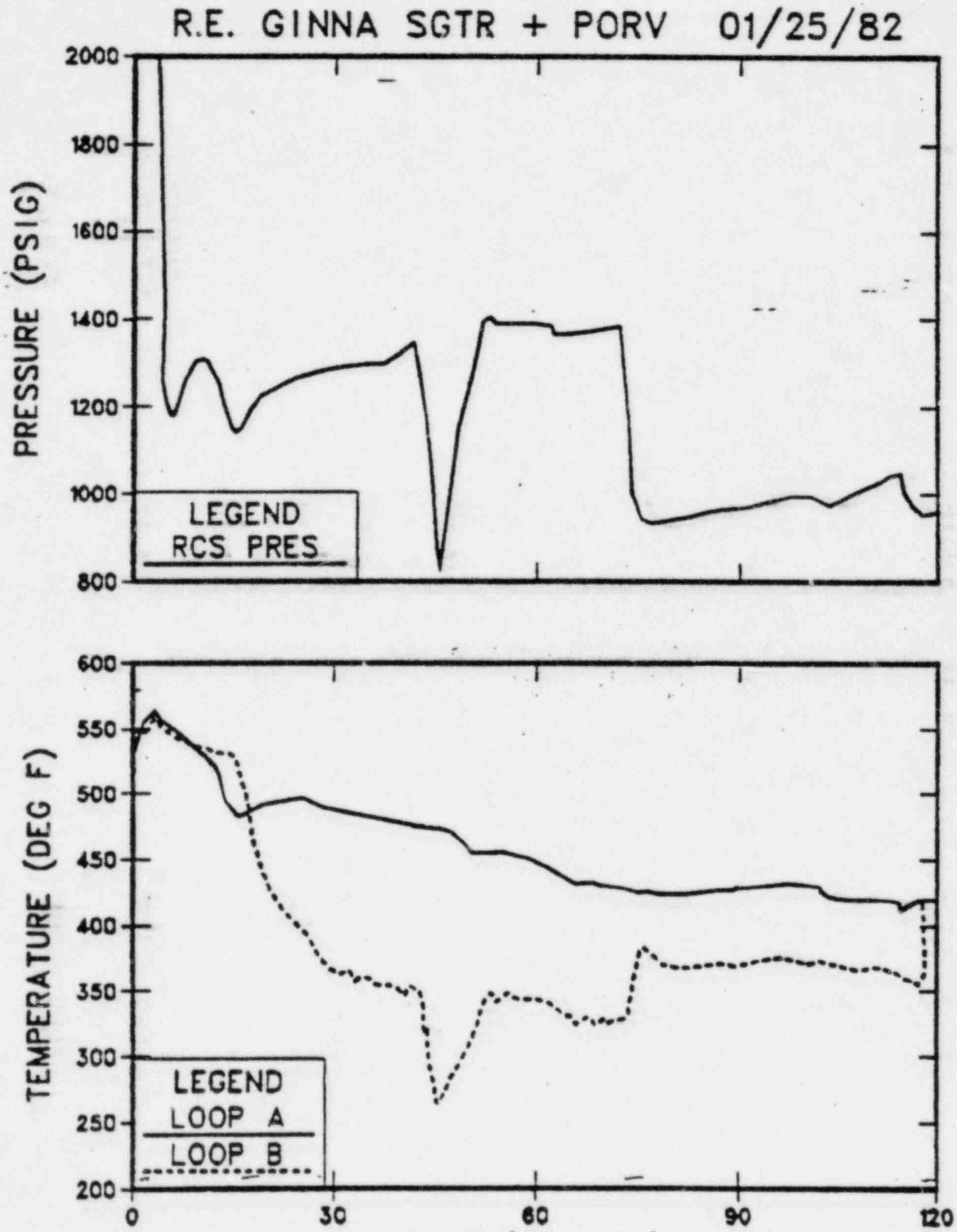

FIGURE 2-11

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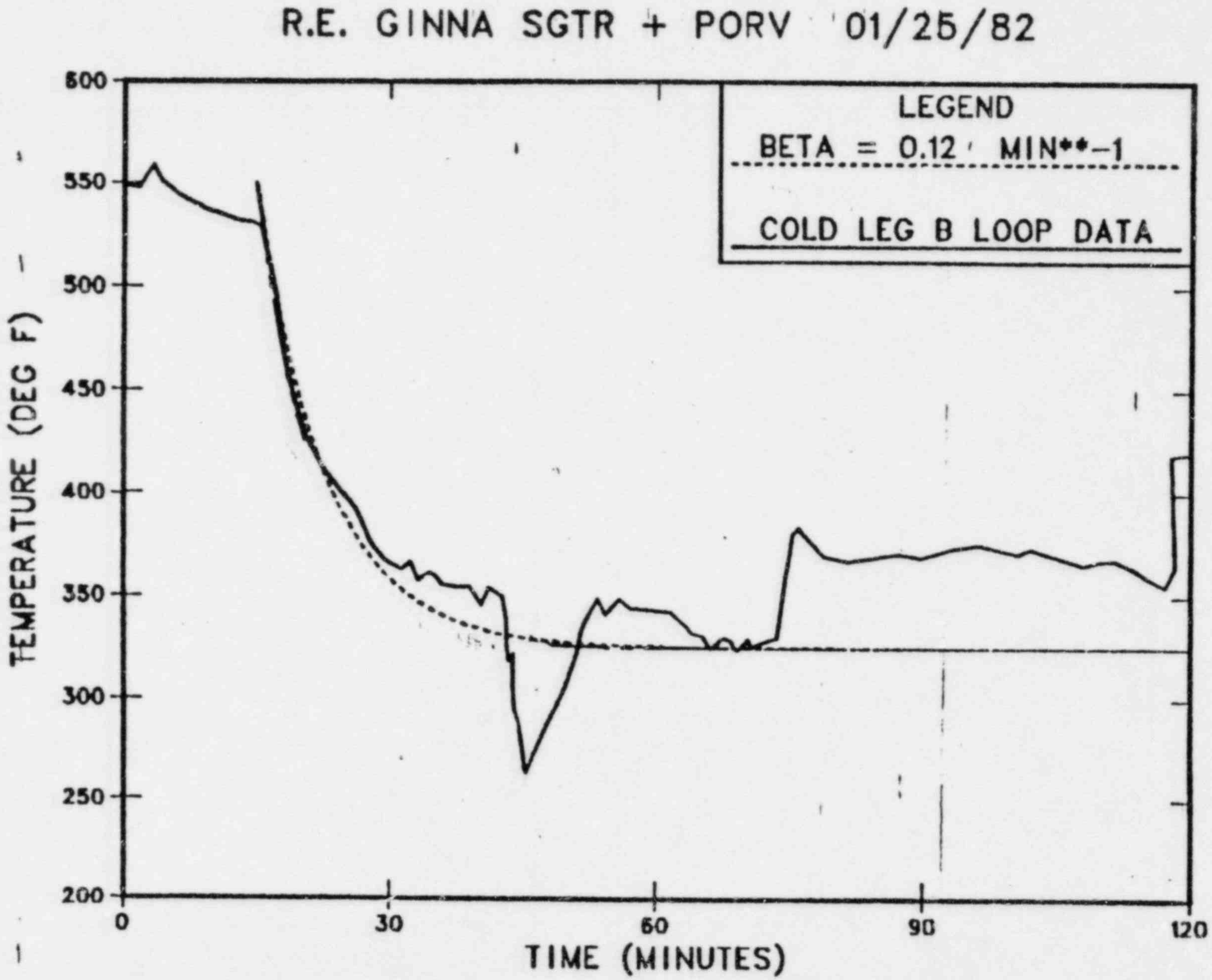

FIGURE 2-12

PTS PRECURSOR EVENTS FINAL TEMPERATURES
32 EVENTS IDENTIFIED

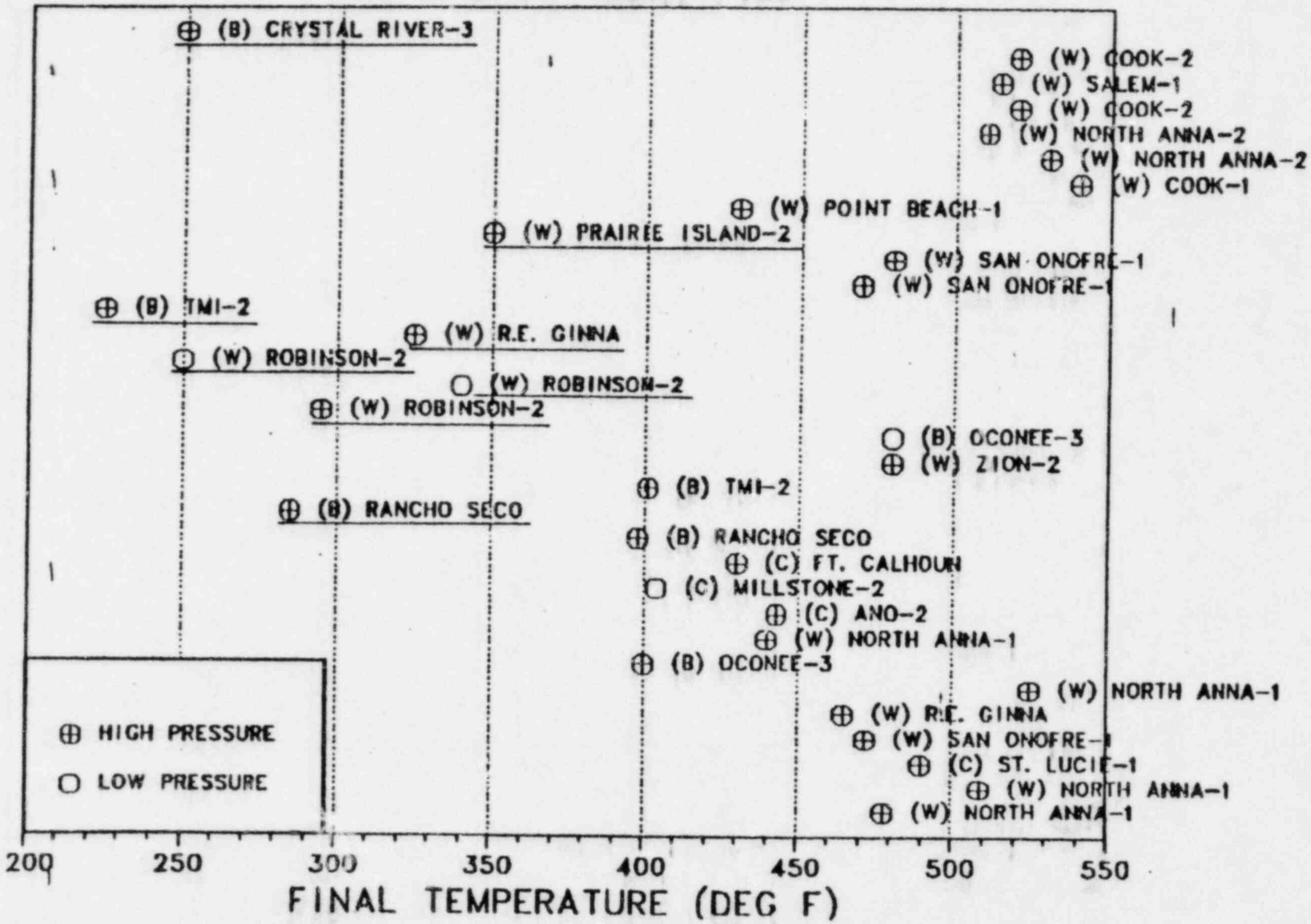

FIGURE 2-13

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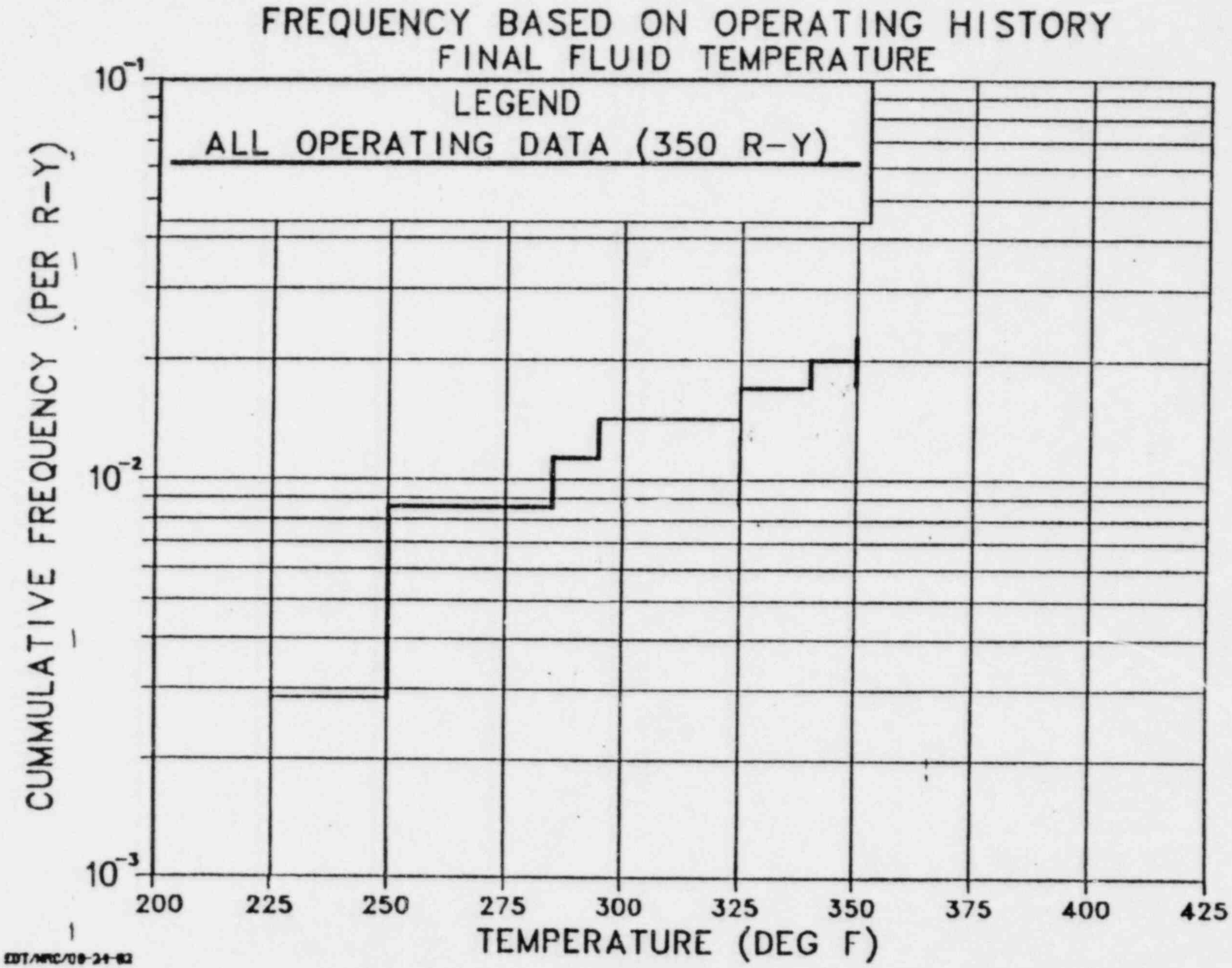

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FIGURE 2-14

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3. DETERMINISTIC FRACTURE MECHANICS ANALYSES

3.1 Fracture Mechanics Discussion

The calculations reported in this section are used to analyze the response of a reactor pressure vessel (RPV) to an overcooling transient. The input information includes (1) pressure and temperature of the reactor coolant as a function of time, obtained from thermal-hydraulic calculations; (2) materials properties, including temperature and irradiation effects; and (3) an actual or assumed initial flaw. Vessel integrity analyses, the results of which are reported in this document, include a determination of the temperature distribution across the vessel wall versus time, the thermal stresses as a consequence of this temperature distribution, as well as fracture mechanics results. Thus, the term "fracture mechanics analysis" used in this section (FM) really means vessel integrity analysis because it includes heat transfer and stress analysis. The stresses considered are those as a result of pressure and other causes as well as thermal stresses.

Once the stress distribution is determined as a function of time and position, FM examines the behavior of prexisting cracks (postulated or real) in this stress field. For specific crack geometries, a stress intensity factor, K_I is calculated and compared to a material toughness property, K_{Ic}. When K_I exceeds K_{Ic} for a specified crack, the crack will initiate, i.e., grow deeper into the metal. K_I for the crack then increases until it reaches a value equal to K_{Ia} which is another material property. The crack then arrests, i.e, stops growing larger. The material properties (K_{Ic} and K_{Ia}) vary with temperature and degree of irradiation damage and hence are a function of time and depth into the vessel wall.

FM alogrithms consider these factors. For pressurized thermal shock (PTS) evaluations, linear elastic fracture mechanics (LEFM) is used because, at the temperatures involved, the metal is at less than its maximum or upper shelf toughness. Illustrations of typical temperature, stress and stress intensity factor distributions within the vessel wall at different times during the transient are shown in Figure 3-1 (a), (b) and (c) respectively.

The quantities K_{Ic}, the vessel toughness that determines crack initiation, and K_{Ia} the toughness at crack arrest, also vary with position and time, since they are functions of irradiation and temperature. When K_I exceeds the value of K_{Ic} at the location of the tip of the flaw, crack initiation is expected, if warm prestressing is not effective (warm prestressing is discussed below and in Appendix D). ıhe crack would then grow to a depth where K_I equals the value of K_{Ia} at the t ɿ ᶜᶠ tℓe growing crack. For some transients, metals properties, and flaws, K_I will remain above K_{Ia} and the crack will go through the vessel wall without arrest.

Similar results would occur for a circumferentially oriented crack except that arrest will generally occur at shallower depths. It should be noted that the stress intensity factor, K_I, for long axial cracks is higher than for long circumferential cracks, especially for cracks that extend relatively deep into the vessel wall.

Equival ,ᵗ calculations are made as a function of time in the transient, and the resu'ᶜs ross-plotted on a critical crack depth diagram. From this diagram, the behavior of a crack versus time for a particular PTS scenario can be determined. Such a diagram is shown in Figure 3-2.

Warm prestressing (WPS) is a phenomenon that can inhibit or prevent crack initiation even though the calculated stress intensity factor, K_I, becomes greater than the material toughness parameter, K_{Ic} at the time and location of the flaw tip. For WPS to be effective, K_I must be at less than a previous maximum value at the time K_I becomes equal to or greater than K_{Ic}. This can occur if K_I is monotonically decreasing as the metal cools causing K_{Ic} to decrease. When the course of a PTS transient can be described with confidence, the time behavior of K_I can be evaluated for determining whether WPS occurs. For generic studies, however, wherein the pressure variation versus time cannot be unambiguously defined, the NRC does not assume the benefit of WPS.
In general, K_I will increase after its initial peak only due to an increase in pressure.

3.2 Description of FM Computer Analysis Programs

The NRC staff utilized its own in-house FM program in performing heat transfer, stress and fracture mechanics analyses related to pressurized thermal shock (PTS) and has also relied on ORNL to supplement the staff analyses by use of the OCA program as reported later in this section. The NRC program is also utilized as the deterministic portion of the VISA program in performing the probabilistic fracture mechanics analyses discussed in Section 7. The NRC and ORNL programs are very similar, as described in Appendix D. Analytical results utilizing these programs for specific PTS scenarios have been compared and found to be in close agreement. Similar comparisons have been made with results of industry analyses. We conclude that the analytical methods used by the NRC, ORNL and the vendors yield essentially the same results if all input assumptions are the same. Differing conclusions result primarily from various assumptions regarding input parameters.

When material properties and the transient are known, fracture mechanics procedures can predict crack behavior quite well as demonstrated by comparison with a wide variety of exeriments. The Heavy Section Steel Technology research program has included hundreds of irradiated test samples, plus model vessels tested at low temperatures to include brittle and transition behavior. Tests have included thermal shock, and plans for the near future include combined pressure and thermal stresses.

Westinghouse vs. NRC Crack Arrest Model - When a crack initiates and grows deeper into a reactor vessel wall, the shape it becomes depends on its initial shape, the stress intensity factor along the crack front and the relative toughness of the metal in which it is growing. Thermal stress analyses for typical PTS transients result in higher tensile stresses at the cooled surface where the metal is colder and hence less tough than deeper into the wall. Based on analyses where cladding effects are neglected and on thermal shock experiments, cracks tend to grow in length prior to growing deeper. In other words, the cracks become relatively long. For this reason, the NRC postulates long cracks at the time of arrest regardless of the original postulated crack geometry.

Discussions with Westinghouse personnel indicated that their analyses assumed a self-similar crack shape with a length-to-depth ratio of six during crack growth and at arrest even though their two-flaw model description was thought to indicate otherwise. The staff does not accept the Westinghouse assumption for the reasons discussed above. Subsequently, Westinghouse has utilized the same assumptions as the staff and finds that then their results are essentially the same as those of the NRC. The original differences in the models resulted in significant differences in critical RT_{NDT} at crack arrest. In view of the importance of this matter, the staff has consulted with recognized exerts in this field who have agreed that, although the NRC model is somewhat conservative, it is more realistic than the original Westinghouse model.

3.3 Determination of K_{Ic} and K_{Ia}

The fracture analyses performed by utilities, vendors and the NRC have all utilized the values of K_{Ic} and K_{Ia} given in Section XI of the ASME Code and reproduced in Appendix D. The Code values are bounds on the conservative (low) side of experimentally determined toughness values. They have been correlated using the relative temperature, T minus RT_{NDT}, which is the reference temperature, nil-ductility transition.

RT_{NDT} is defined in Appendix D. It is a reference temperature that is used to characterize the transition in material properties, from ductile to brittle, that takes place as the temperature is decreased. Actually, the transition in properties is gradual, taking place over a temperature range of 100°F. The use of the relative temperature, $T-RT_{NDT}$ has been shown to allow correlation of experimental toughness data in RPV materials at various temperatures, irradiation states, and stress conditions. The Heavy Section Steel Technology fracture experimental data also show the $T-RT_{NDT}$ correlation.

The initial value of RT_{NDT} in a new, unirradiated vessel is quite low (0°F), but increases with irradiation. The NRC staff's method for estimating the initial RT_{NDT} and the change in RT_{NDT} caused by irradiation for a given vessel are given in Section 5 and Appendix E of this report. Estimates are given for RT_{NDT} at the inside surface of the vessel wall (at the clad-base metal interface) for the critical locations, which are almost always the welds, either a longitudinal weld or a circumferential weld in the beltline. The attenuation of RT_{NDT} through the vessel wall is then calculated to get K_{Ic} and K_{Ia} at the tips of postulated cracks (see Appendix D).

3.4 Generic Deterministic Studies of Crack Initiation

Using the models described in the preceding sections and in Appendix D, NRC and ORNL have performed a variety of deterministic FM analyses. The results are given in Appendix D and are summarized here.

3.4.1 Stylized Transients

The stylized transients used are described in Section 2.1, characterized by constant pressure, P, initial water temperature of 550°F, final water temperature, T_f, and exponential decay constant β, minutes^{-1}. The water temperature is assumed to be uniform over the inner surface of the vessel. A constant heat transfer coefficient, h, is used for the water-metal interface. An infinitely long through-clad flaw is assumed to exist on the inner surface of the vessel wall.

3.4.2 Crack Initiation for Stylized Transients

At the request of the NRC staff, ORNL performed a series of analyses with different assumed values of T_f, β, and P assuming that crack arrest and WPS were not effective. The results are plotted as a series of curves of pressure versus $T_f - RT_{NDT}$, an example of which is Figure 3-3. Other examples are provided in Appendix D. Note that from these diagrams, the thresholds of crack initiation can be determined. Thus, for a specific vessel RT_{NDT} and a given β and T_f, it is possible to determine the limiting pressure to avoid crack initiation.

Utilizing Figure 3-3, it is possible to relate approximately the RT_{NDT} that a vessel must possess to avoid crack initiation for a given transient to the final temperature of the transient. For conservatism when considering a generalized event, it is assumed that a moderately fast cooldown has occurrred ($\beta = 0.15\ min^{-1}$) and that full pressure (2300 psig) exists in the vessel since there is no assurance that it will be possible to take credit for automatic or manual pressure reduction. Thus, the upper right-hand portion of the figure is used, and it is seen that, for T_f of 250 to 300°F, and for longitudinal flaws, final temperatures approximately 5°F above RT_{NDT} are acceptable, but as one proceeds to more severe cooldown events (T_f = 150°F) the final temperature must stay as much as 20°F above RT_{NDT}.

3.4.3 Sensitivity Studies

In addition to the many uncertainties regarding PTS scenarios such as the temperature and pressure profiles versus time, the degree of mixing of cold with warm water, etc., parametric uncertainties in the stress and fracture mechanics analyses become significant when the cooldown temperature, T_f, is approximately equal to RT_{NDT} because small changes in assumptions can influence whether or not crack initiation is predicted. The staff performed analyses similar to those by ORNL with various assumptions as to crack shape and orientation with and without cladding-induced stresses and for different models for fluence attenuation through the wall in order to determine the effects of these assumptions. (Cladding stresses are induced because of the different coefficients of expansion of the stainless steel cladding and the carbon steel of the vessel wall.)

Sensitivity studies used a base case with T_f = 250°F, $\beta = 0.15\ min^{-1}$, and considered various values of P. Some results are shown in Figure 3-4. The threshold value of RT_{NDT} for crack initiation is given.

The importance of the pressure (assumed constant in these stylized transients) is shown in Figure 3-4. The critical RT_{NDT} value of 245°F for a 2250 psig transient is increased to 290°F if the pressure can be limited to 500 psig during the time interval of high thermal stresses.

Cladding-no cladding comparisons (Figure 3-4) show a decrease of 10°F in critical RT_{NDT} when the cladding effect is included.

For this reference transient, with RT_{NDT} = 294°F, the pressure has to be reduced to near saturation within about 30 minutes to avoid crack initiation. However, if the pressure remains constant after an initial drop or monotonically decreases with time for this stylized transient, WPS at about 18 minutes would be effective, and crack initiation would not occur. The measured temperatures and pressure experienced in actual overcooling transients (Section 2.2) show ups and downs, some of which would be predicted to negate WPS.

The orientation of postulated cracks affect their behavior during a PTS event. For a specified thermal transient and the same shape and depth of a pre-existing crack, the thermal stress intensity factor for a circumferential orientation is less than that for an axial orientation. The difference is minimal for shallow cracks but becomes significant for deep cracks. The reason for this difference is the relative stiffness of the vessel wall in the two directions which is accounted for in the fracture mechanics and analytical model. For typical reactor vessels, the axial and circumferential thermal stresses are essentially equal in magnitude. Axial pressure stresses, on the other hand, are about a factor of two lower than tangential stresses; the axial stresses affecting circumferential cracks and tangential stresses affecting axial cracks. Thus, the total axial PTS stresses are equal to or less than the tangential stresses depending on the system pressure. For the above reasons, circumferential cracks are more tolerant of PTS events.

The difference between the two orientations in terms of critical RT_{NDT} depends on the specific PTS scenario. A limited number of examples described in

Appendix D show that for relatively severe postulated transients, the RT_{NDT} difference is about 30°F for crack initiation and the order of 100°F for crack arrest situations. The higher RT_{NDT}s are for circumferential cracks.

Detailed comparisons of Westinghouse and NRC calculations show the following sensitivities:

Assumptions	Change in critical value of RT_{NDT}, °F
(a) Cladding vs. no clad stress	10
(b) Continuous flaw for intiation vs. elliptical flaw (a/c = 1/3)	20
(c) h = 300 BTU/hr-ft²-°F vs. Westinghouse free convection correlation	15

The above tabulated assumption differences account for a total variation of about 45° in critical RT_{NDT} between staff analyses and those of Westinghouse, with the Westinghouse assumptions giving higher values of critical RT_{NDT} than the NRC assumptions. The NRC staff is inclined to accept the Westinghouse assumptions (b) and (c) as more nearly realistic than the NRC staff assumptions, but believes that the cladding effect should be included in accordance with the NRC assumption.

Such "fine tuning" details are relevant to all calculations but are believed by the NRC staff to be within the error band of such calculations. Only for limiting transients like the small break LOCA with stagnated circulation (Section 6 and Appendix G) are these minor corrections important; they are taken into consideration there.

3.4.4 Crack Arrest

For much more severe thermal transients, crack initiation may occur due to high thermal stresses. In this case it is appropriate to consider the potential for crack arrest. Figure 3-2 is a schematic representation of a critical crack depth diagram to illustrate the analytical model used by the staff for determining acceptable arrest criteria. For a small crack, the path of the transient is shown by the dotted line in Figure 3-2. An initial flaw of critical depth is shown; smaller or larger flaws would initiate later. After initiation, the crack runs until $K_I = K_{Ia}$ as shown, then arrests.

Although the K_{Ia} arrest value becomes quite high at larger times, the model in its simple form does not include ductile tearing. For this reason, a maximum allowable value of K_{Ia} is imposed, the "upper shelf" value. For NRC calculations, an upper shelf toughness of 200 ksi $(in.)^{1/2}$ is assumed; however, higher or lower values may be more appropriate for a specific material.

The vessel remains intact if WPS prevents crack initiation or, if a crack initiates, it arrests, and for crack depths such that K_I is lower than the upper shelf value.

Since the total stress intensity is the sum of pressure and thermal contributions, if the thermal value is known at the time of WPS, a diagram like Figure 3-2 gives the maximum pressure allowable for crack arrest. When the thermal stress intensity factor is known at the time of WPS, the maximum pressure is determined such that arrest will occur at or before the time of WPS and for crack depths such that K_I is below the upper shelf curve. The limiting case is shown as point "A" in the figure.

For transients that have actually occurred, it is not necessary to make assumptions of the stylized transients of Section 2.1 and the preceding sections of this chapter. Rather it is possible to perform fracture mechanics calculations for the pressure and temperature history as it actually occurred. These calculations were performed assuming a range of RT_{NDT} values, for the eight overcooling transients experienced to date and described in Section 2.2. Thus, it was possible to predict the limiting vessel material condition (critical RT_{NDT} or RT_c) necessary to prevent vessel failure for each of these experienced transients. The results are shown in Table 3.1, for longitudinal cracks, together with results from Section 2.2 of estimating T_f, β, and P for stylized transients to approximate the course of the events actually experienced.

It is seen that, with the TMI exception (where cooldown must stop 16°F above RT_{NDT}) cooldowns to estimated values of T_f from 10°F to 100°F below RT_{NDT} are not predicted to fail the vessel.

When compared with the results of the stylized procedure presented in Section 3.4.2, which showed that cooldown should stop 5°F to 20°F above RT_{NDT}, this result shows some of the conservatism generally present in the stylized procedure compared to direct calculations of critical RT_{NDT} for experienced events.

Table 3.1

Plant and Year	$T_f(^\circ F)$	$\beta(min^{-1})$	P(psig)	RT_c
TMI	225	0.04	2300	209
HBR '75	250	0.02	500	354
Ginna	325	0.12	1400	378
Rancho Seco	285	0.10	2300	295
HBR '70	295	0.08	2000	321
HBR '72	340	0.015	1000	381
Crystal River	250(?)	0.10	2300	(250)
Prairie Island	350	0.10	1000	(400)

RT_c is the RT_{NDT} that is necessary to prevent crack initiation based on actual Pressure and Temperature variations with time. Stylized values of T_f, β, and P are shown from Section 2 but were not used in these calculations to determine RT_c.

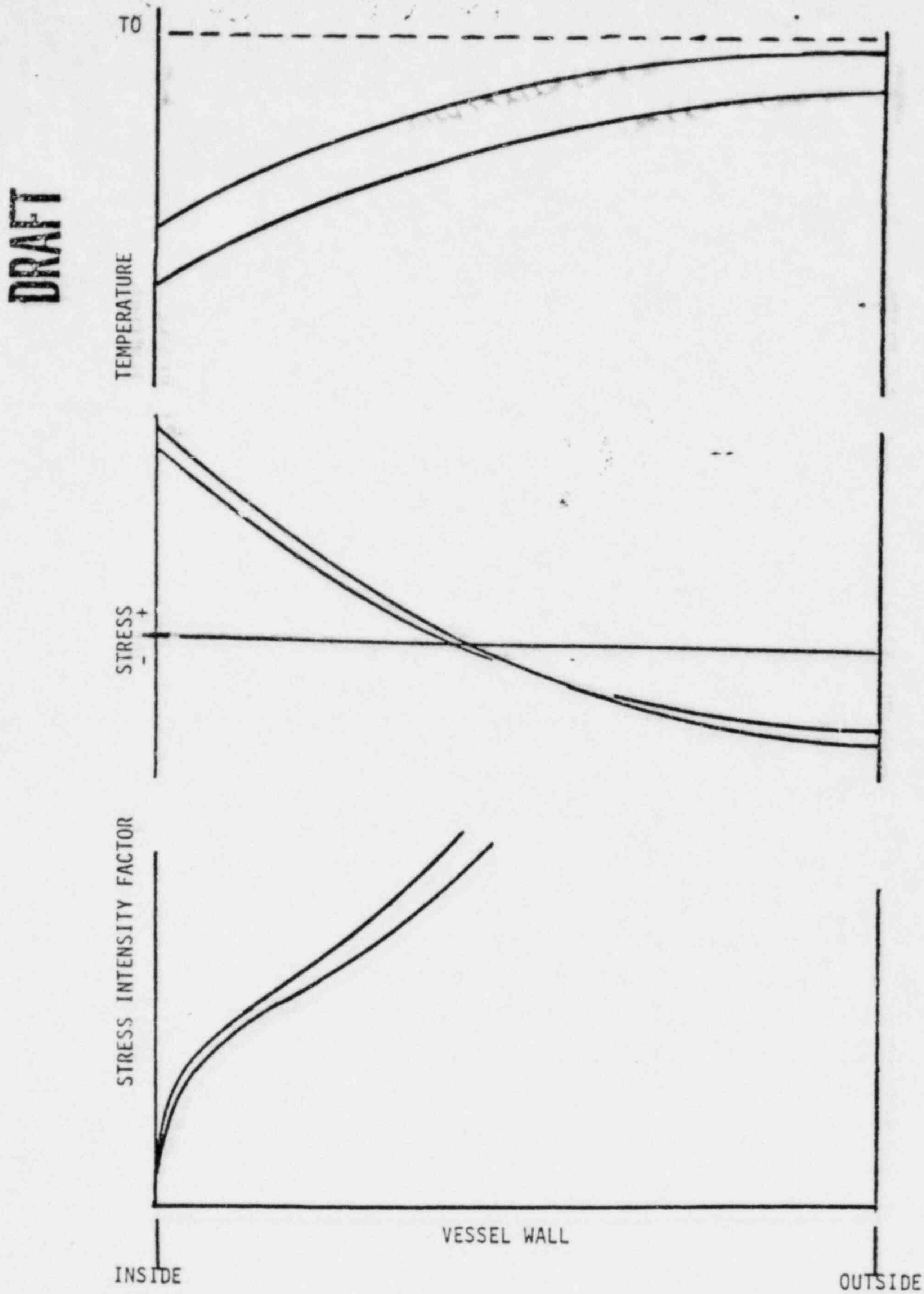

FIGURE 3-1
DEVELOPMENT OF PTS TRANSIENT
IN A VESSEL WALL

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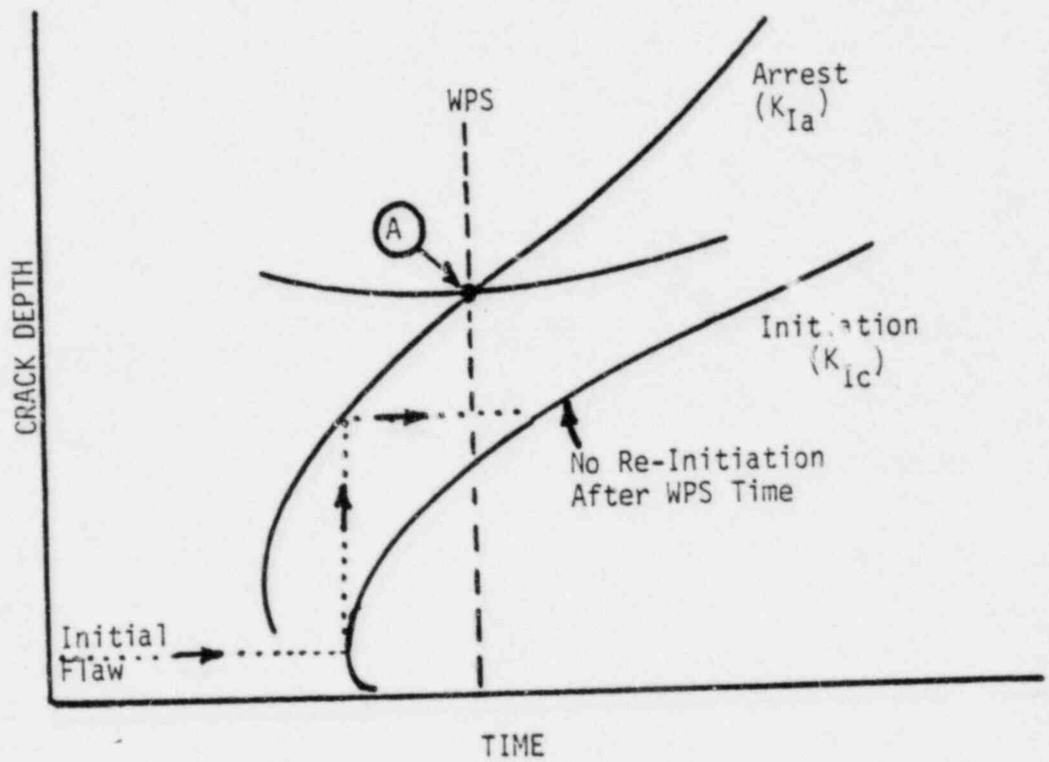
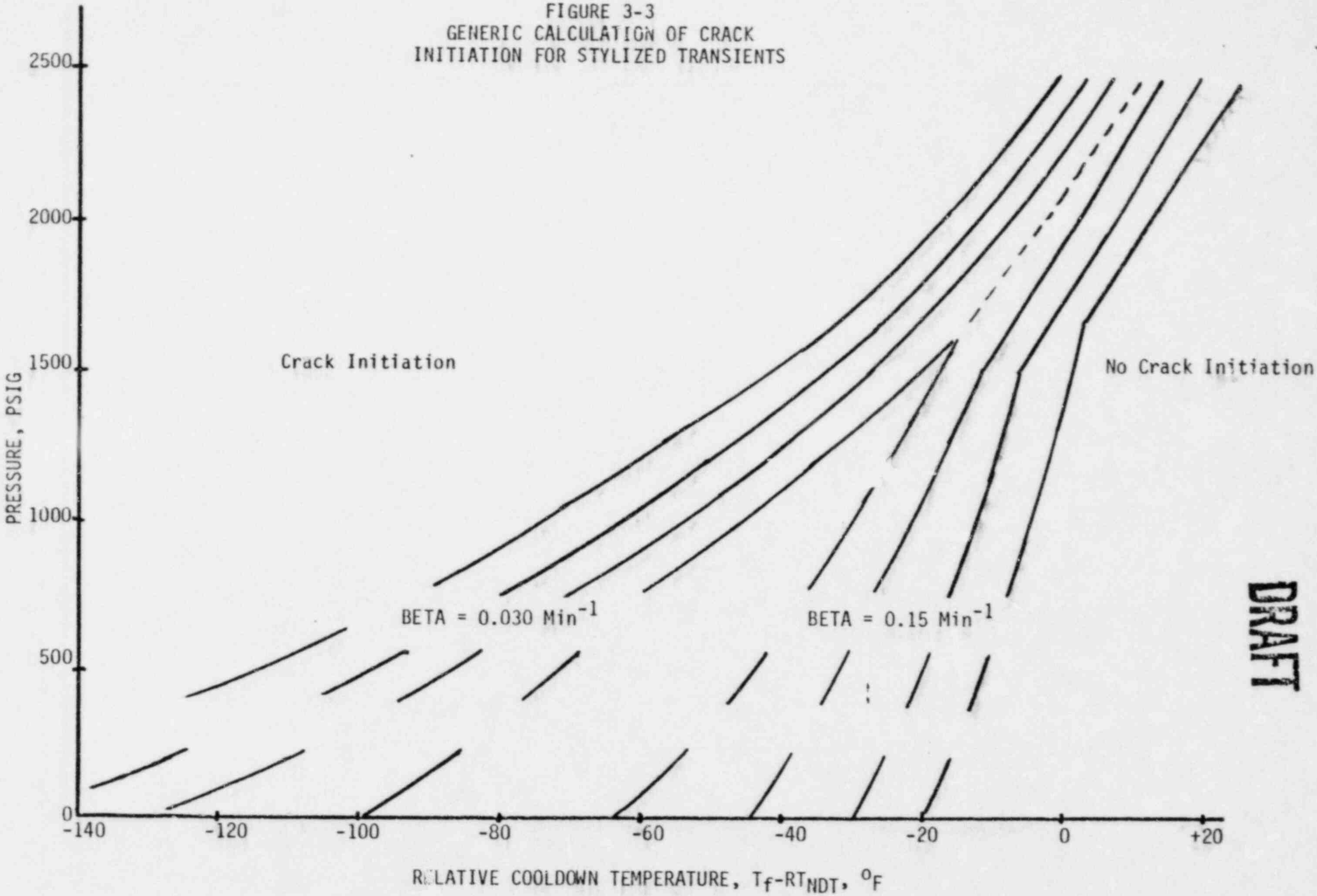


FIGURE 3-2, NRC CRACK
INITIATION AND ARREST
MODEL

FIGURE 3-3
GENERIC CALCULATION OF CRACK
INITIATION FOR STYLIZED TRANSIENTS



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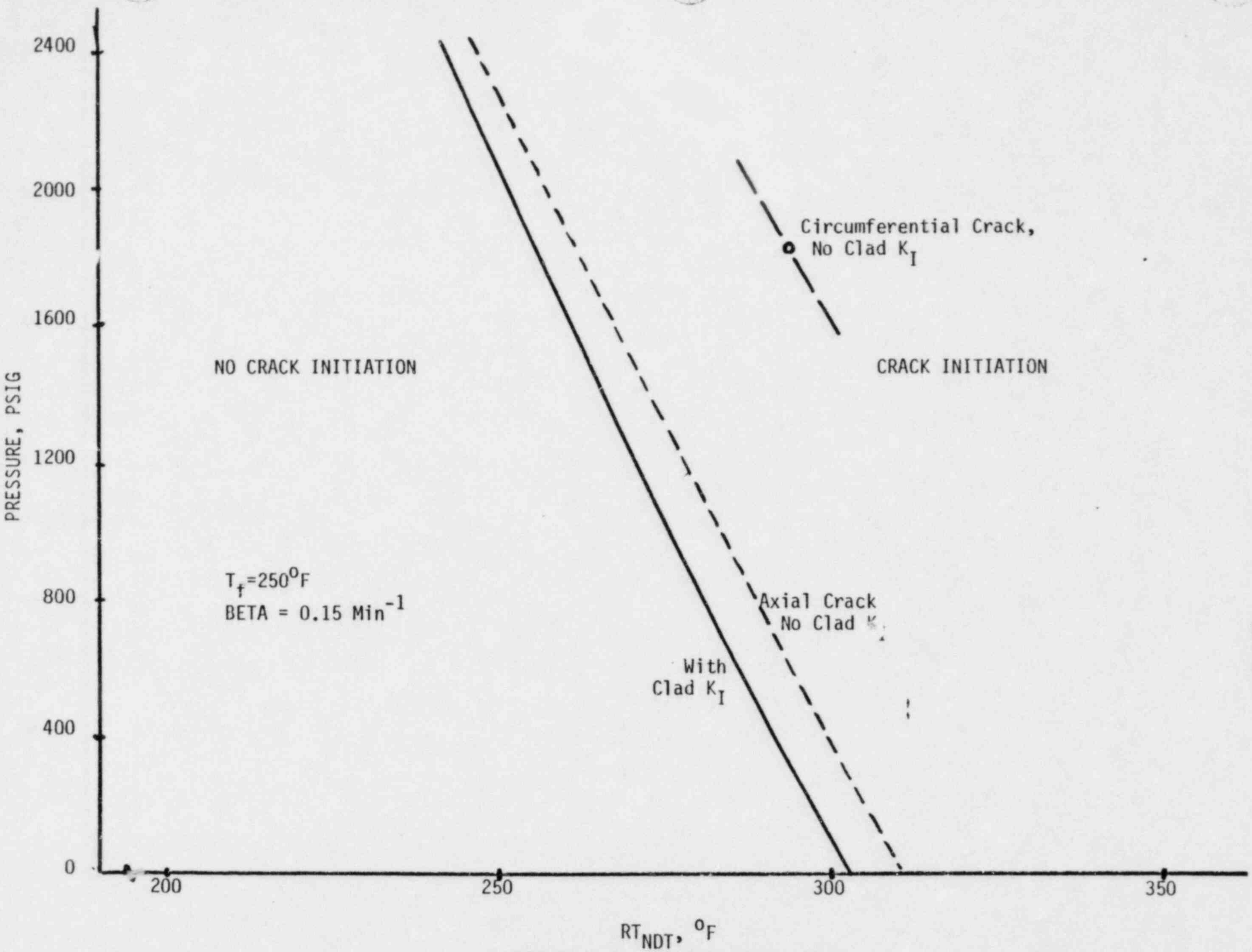

FIGURE 3-4 SENSITIVITY STUDIES

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4. SELECTION OF SCREENING CRITERION

The experienced events discussed in Section 3 were used as the basis for selecting a RT_{NDT} screening value, as described in this section.

The events were listed on Table 3.1 in terms of the cool down temperature (T_f, first column) and in terms of the critical RT_{NDT} described in Section 3.4.4 (RT_c, last column). Based on about 330 total PWR reactor years operating experience in the United States, the T_f values for the eight events can be used to develop a plot of the cumulative frequency per reactor year of events with final temperatures lower than the temperature shown. This was done in Figure 2-14. Similarly, the RT_c results of Table 3.1 were used to develop a plot of the cumulative frequency of events versus the RT_c for which the deterministic fracture mechanics calculations predict crack extension will occur (Figure 4-1).

From examination of Figure 4-1, it appears that for a reactor vessel with an actual RT_{NDT} value of about 270°F, the deterministic fracture mechanics calculations described in Section 3 above would predict no crack initiation from pre-existing flaws in axial welds for overcooling events that have been experienced with a frequency of about 6×10^{-3} per reactor-year or larger. As discussed in Section 3, the corresponding value for circumferential welds for events in this temperature range is at least 30°F higher, due to the difference in stress intensity factors.

The staff proposes that RT_{NDT} values of 270°F for axial welds, and 300°F for circumferential welds be used as screening criteria to determine when plant-specific evaluations should be performed for operating plants. It is recognized that the choice of a criterion for action on the basis of generic deterministic fracture mechanics analyses and the limited number of overcooling events that have occurred is subject to many uncertainties and assumptions, some of which are conservative, and some are nonconservative.

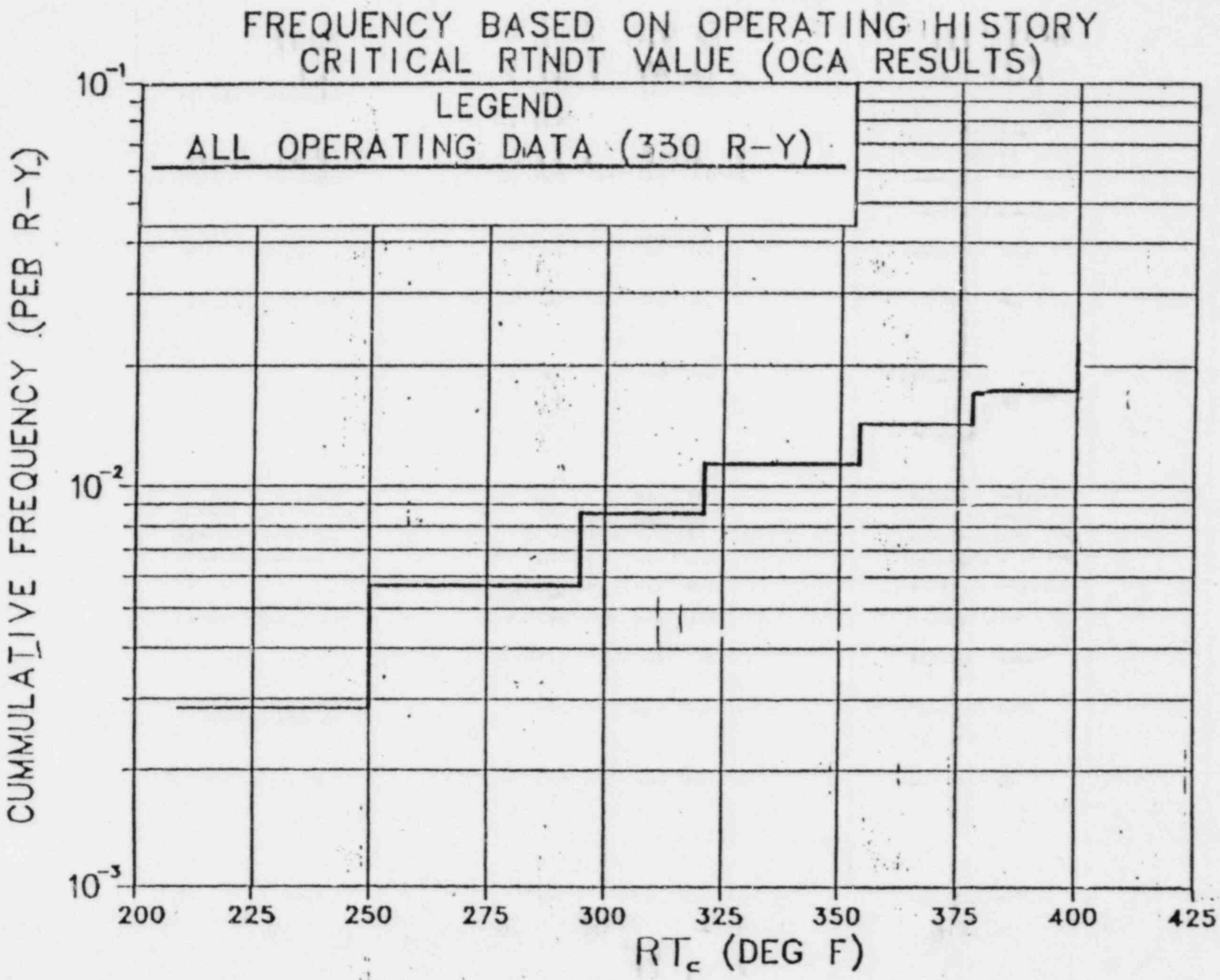

Figure 4-1

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The use of an experience base of only eight events to develop an expected frequency distribution clearly yields values with large uncertainties and does not take account of lower probability events that have not yet occurred. In addition, the temperature histories used in the fracture mechanics calculations were measured in the cold leg piping, whereas the temperature of interest (at highly irradiated welds) is in the reactor vessel downcomer which might have been colder.

The fracture mechanics calculations assume that flaws of critical size are present at the limiting welds (those with highest RT_{NDT}). This is clearly a conservatism in the analysis, but one which cannot be quantified.

Because the intent is to select a screening criterion generically, covering a wide range of transient sequences, the analysis does not take credit for the warm prestress phenomenon which would be effective in many actual transient sequences. On the other hand, no account has been taken of the effects of weld residual stresses.

Perhaps the most significant uncertainty in the treatment described thus far is that there are known low frequency potential overcooling events much more severe than those that have been observed. Because these events have not occurred, they have not been taken into account in the frequency distribution used.

Because of all of the nonquantified uncertainties noted above, the staff has also examined what insights can be gained from calculations of the characteristics of various postulated overcooling events and estimates of their expected frequency of occurrence; and from a probabilistic study of the fracture mechanics calculations. These considerations are described in Sections 6, 7 and 8.

5. DETERMINATION OF RT_{NDT}

5.1 Introduction

If as is recommended in this report, a value of RT_{NDT} is selected to serve as a screening criterion to determine the timing of plant specific evaluations of possible needed modifications to provide protection against pressurized thermal shock events, then it is important for the staff to select a suitably conservative and uniform method for determining the plant-specific values of RT_{NDT} at a given time. During the service life of the reactor vessel the initial value of RT_{NDT} ($RT_{NDT}(o)$) increases because of neutron irradiation by an amount ΔRT_{NDT} which depends on fluence and materials properties. The initial value, $RT_{NDT}(o)$, is determined from materials tests made at the time the vessel was fabricated. The change ΔRT_{NDT} is determined from fluence measurements and calculations and from trend curves, based on tests of irradiated specimens, that predict the effects of neutron irradiation. There are a number of uncertainties in the estimation of both $RT_{NDT}(o)$ and ΔRT_{NDT} and it is important to establish a prescribed method for calculation with a degree of conservatism appropriate for use in connection with the screening criterion. The methods described in this section were selected based on the recommendations of an NRC Working Group of staff members and consultants (Reference 5.1). The methods and the bases for them are presented in greater detail in Appendix E of this report. The uncertainties in estimates of fluence are discussed in Appendix F.

5.2 Estimation of Initial RT_{NDT}

The summer 1972 Addenda to Section III of the ASME Boiler and Pressure Vessel Code contained the first requirements for measurements to be made at the time of fabrication of RT_{NDT} for the plates, forgings, and welds that make up the reactor vessel. Two types of tests are required--drop-weight tests and Charpy tests. However, most of the vessels of concern regarding PTS were fabricated in the 1960's when only Charpy tests were required.

Typically, the data available comprise three Charpy tests at 10°F for each plate, forging and weld, complete Charpy curves for the surveillance weld and base materials, and in cases where the base material was controlling, some drop weight data on archive or surveillance material. In the past, the NRC has used the guidelines given in the Standard Review Plan Branch Technical Position MTEB 5-2, to obtain an estimate of initial RT_{NDT}. In summary, those guidelines were to use the temperature corresponding to a Charpy 30 ft.-lb. level, but not lower than 0°F. The Charpy curves from the surveillance tests were used to guide any extrapolation needed to get the 30 ft.-lb. temperature from the three test results at +10°F. Such estimates are not very satisfactory, however. They are overly conservative for some cases.

From compilations of data obtained subsequent to the time the vessels in question were made, it is possible to divide the welds into two groups according to the weld flux used, and to develop a mean value and a standard deviation (sigma) for the generic data. One must then decide if it is prudent to use the mean generic value as the best estimate for the vessel welds in question. Except for some archive material, the welds that are represented in the data base were made at a later time than the vessel welds. There may have been some differences in weld chemistry or welding practice. Furthermore, even if there were actual RT_{NDT} values for the vessel weld in question, the samples would come from weld metal qualification welds, not from actual vessel weld prolongations and not from full thickness test pieces.

The staff has concluded that a suitably conservative method for estimating the initial value of RT_{NDT} for use in comparisons with the screening criteria proposed in Section 4 is to use the mean value as described above with an adjustment for the standard deviation as discussed in Section 5.4 below. Additional discussion and details regarding the estimation of the initial RT_{NDT} are presented in Appendix E and in Reference 5.1.

5.3 Estimates of the Shift in RT_{NDT} Due to Radiation (ΔRT_{NDT})

Two methods are generally used to estimate the shift in RT_{NDT} caused by neutron irradiation of the pressure vessel: (1) tests of metallurgical surveillance specimens irradiated in the reactor vessel, and (2) "trend curves" of ΔRT_{NDT} as a function of weld chemistry and neutron flux developed from analyses of a large number of irradiated specimens.

Many older operating plants have withdrawn and tested surveillance specimens. However, there are problems associated with using individual surveillance results as the sole source of information about a plant. First, the surveillance weld often does not match the critical vessel weld exactly, i.e., the weld wire heat numbers are different. A broader problem is that caused by scatter in the ΔRT_{NDT} data. This results in part from the fact that ΔRT_{NDT} is the difference between the curves for irradiated and unirradiated material, both of which were fitted to data that typically show considerable scatter. Thus, there is a preference for the use of trend curves, instead of individual surveillance data.

Since publication in April 1977, Regulatory Guide 1.99 Rev. 1 contains the procedure recommended by the NRC to obtain ΔRT_{NDT} as a function of chemistry and neutron fluence. Copper was the dominant residual element in the chemistry term (the other was phosphorus).

Critics of Regulatory Guide 1.99 have asserted that (a) the curves are too conservative at high fluences, especially for low-nickel materials, and (b) the phosphorus term is not supported by recent studies such as that of the Metal Properties Council (Reference 5.2). Evidence has been accumulating for several years that low-nickel materials are less sensitive to neutron radiation. When the PWR surveillance data base was analyzed by the NRC in October 1981, the difference between high- and low-nickel content material was apparent. Westinghouse and CE reported similar findings and presented empirical equations for the low-nickel material. (B&W has no plants with low-nickel materials in the reactor vessel.)

The PWR surveillance data have now been fitted by a multiple regression analysis technique. The work was done at HEDL by George Guthrie (Ref. 5.3). The Guthrie mean curve is as follows:

$$\Delta RT_{NDT} = (-10 + 470\ Cu + 350\ Cu\ Ni)\ (f/10^{19})^{0.27}$$

where: ΔRT_{NDT} = adjustment of reference temperature degrees F

Cu = weight percent copper

Ni = weight percent nickel

f = fluence, n/cm^2 (E>1 MeV)

The standard deviation obtained from the analysis is 24°F.

As shown in Appendix E, the Guthrie mean curve has been compared with a mean curve developed by the Metal Properties Council (MPC) for ASTM Committee E-10 on Nuclear Technology and Applications (Ref. 5.2). The MPC data base contains all of the test reactor and surveillance data that were available in November 1977, and that fit the criteria for material form and irradiation temperature. There is reasonably good agreement between the MPC trend curves and the Guthrie curves, considering that the MPC curves were for a range of nickel content, but were without a nickel term in the equation.

The MPC trend curve did not contain a phosphorus term, because in the regression analysis the addition of a phosphorus term did not produce any significant decrease in the residual variance. In a further study of this finding, the MPC Task Group found a statistically significant relationship of phosphorus content to copper content, i.e., high phosphorus was found with high copper. Thus, their combined effects were represented in the MPC trend curve formulation by a copper term alone.

For high values of copper and nickel contents, the Guthrie mean curve described above gives values higher than those predicted by that part of the Upper Limit Curve of RG 1.99, given by the equation:

$$\Delta RT_{NDT} = 283\ (f/10^{19})^{0.194}$$

Experience has shown that the latter equation bounds the available data.

Therefore, in developing the method for estimating RT_{NDT} values to be compared with the screening criteria proposed in Section 4, the staff recommends that

ΔRT_{NDT} be calculated using a combination of the Guthrie mean curve and the RG 1.99 upper bound curve, with adjustments for the standard deviation as discussed in Section 5.4.

5.4 Recommended Method for Calculation of RT_{NDT}

An NRC Working Group of staff members and consultants reviewed the available information regarding RT_{NDT} determinations and recommended that the following method for calculating RT_{NDT} values for specific reactor vessels be used for comparison with the screening criteria of Section 4 (Ref. 5-1).

The value of RT_{NDT} at the inside surface of the vessel should be taken as the lesser of:

$$RT_{NDT} = RT_{NDT}(0) + \Delta RT_{NDT}(\text{mean}) + 2 \left[\sigma_o^2 + \sigma_\Delta^2\right]^{1/2}$$

$$RT_{NDT} = RT_{NDT}(0) + \Delta RT_{NDT}(\text{RG}) + 2\sigma_o$$

where: $RT_{NDT}(0)$ = the mean value of the initial RT_{NDT} determined as described in Section 5.2 above and in Appendix E.

$\Delta RT_{NDT}(\text{mean})$ = the mean value of RT_{NDT} based on the Guthrie trend curve

$= (-10 + 470\ \text{Cu} + 350\ \text{CuNi})\ (f/10^{19})^{0.27}$

$\Delta RT_{NDT}(\text{RG})$ = the portion of the upper bound curve of Regulatory Guide 1.99 for high values of copper and nickel contents

$= 283\ (f/10^{19})^{0.194}$

σ_o = the standard deviation value from the $RT_{NDT}(0)$ analysis (see detailed discussion in Appendix E)

σ_Δ = the standard deviation for the Guthrie mean curve

$= 24°F$

Cu = weight percent copper

Ni = weight percent nickel

and f = fluence, n/cm^2 (E>1 MeV) (See discussion of fluence uncertainty in Appendix F.)

Note that the second of the two equations above does not include a standard deviation term for $\Delta RT_{NDT}(RG)$ since the Regulatory Guide term used is an upper bound equation.

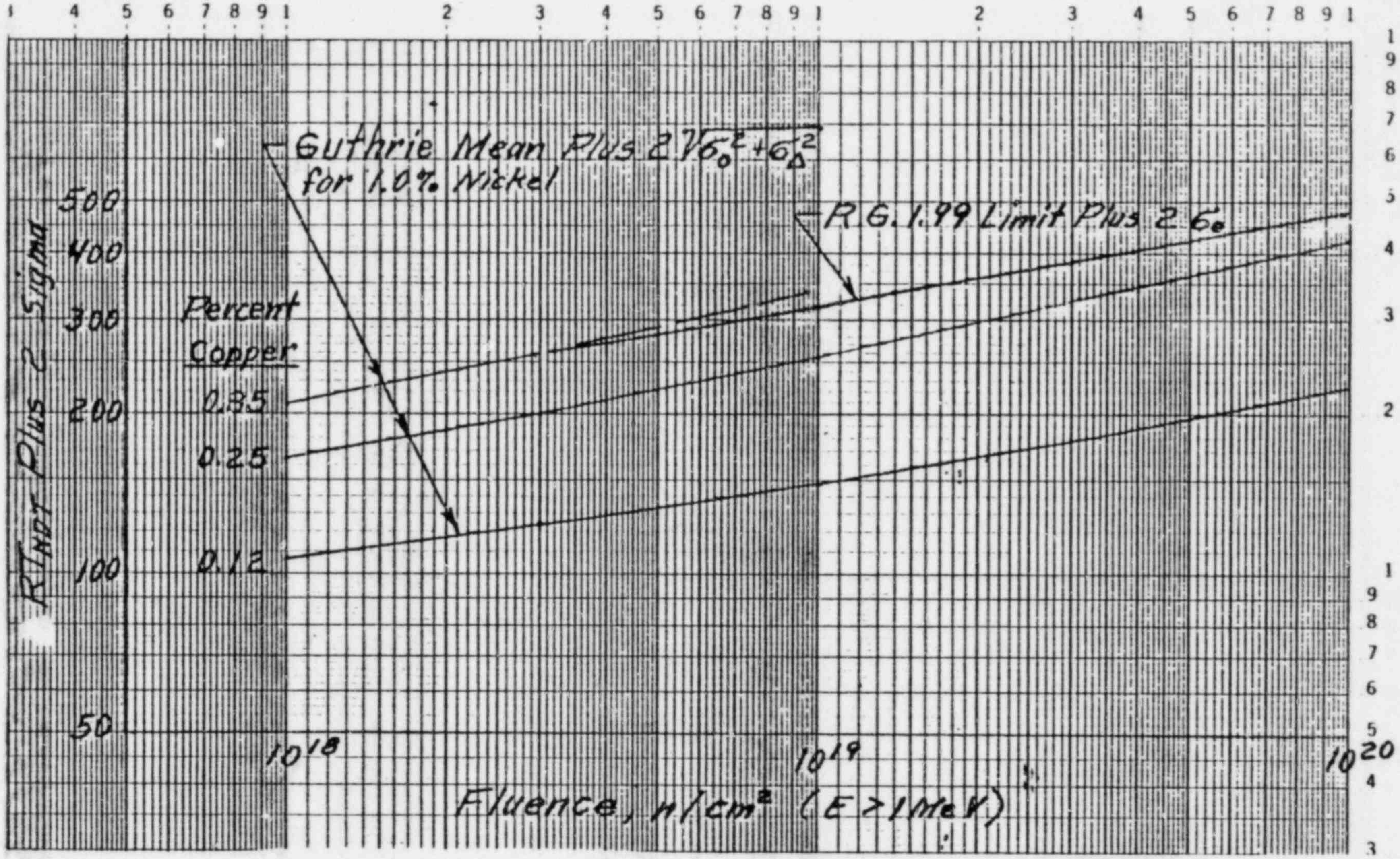
This formulation is plotted in Figure 5-1 for three values of copper content and a nickel content of 1%.

REFERENCES

5.1 Report of the Working Group on RT_{NDT}, Memo, M. Vagins to S. Hanauer, August 30, 1982.

5.2 Prediction of the Shift in the Brittle-Ductile Transition Temperature of LWR Pressure Vessel Materials, Edited by J. J. Koziol, Prepared by a Task Group of Subcommittee 6 of the Metal Properties Council, April 6, 1981 as a Report to ASTM Committee E10.01. To be published by ASTM.

5.3 LWR Pressure Vessel Irradiation Surveillance Dosimetry Quarterly Progress Report, Jan.-Mar. 1982, HEDL-TME 82-18. To be published.



Guthrie Mean Plus $2\sqrt{\sigma_0^2+\sigma_\Delta^2}$
for 1.0% Nickel
R.G. 1.99 Limit Plus 2 σ_e
RT_{NDT} Plus 2 Sigma
500
400
300
200
100
50
Percent
Copper
0.35
0.25
0.12
10^{18}
10^{19}
10^{20}
Fluence, n/cm² (E > 1 MeV)

DRAFT

6. CONSIDERATION OF LOWER PROBABILITY EVENTS

6.1 Identification of Event Sequences with Significant PTS Risk

In order to determine the potential significance of challenges to reactor vessel integrity due to pressurized thermal shock from lower probability events, a systematic approach which identifies all relevant sequences of single and multiple failures from all pertinent initiating events is needed. Event tree techniques are an orderly approach for performing this quantification. Such a study using probabilistic methods and mostly existing PRA data bases was performed by Westinghouse for the Westinghouse Owners Group (WOG). The description and results of this study were submitted to the NRC by the WOG as Reference 6.1. The staff accepts the methodology used in the study to identify event sequences which contribute to risk from pressurized thermal shock and important portions of the discussion presented below have been adapted from Reference 6.1. Although there is agreement with WOG on the structure of the events that should be considered, the staff differs with the WOG in the resulting frequencies for many of the event sequences significant to PTS.

"The approach taken is to first identify the set of all the initiating transients or events which either by themselves or along with succeeding failures could lead to potential challenges to vessel integrity. The sequence of accompanying branching chains of events including component failures and their probabilities is logically traced out in the event trees. The output of the event tree is a set of end states and their frequencies. These end states can then be evaluated for potential challenges to the vessel from pressurized thermal shock. The sum of the frequencies of the end states which are potential challenges is the total frequency per reactor year of vessel integrity challenges summed over all types of initiating events" (Reference 6.1).

Initiating events used in this study are presented in Table 6.1 and include those which either directly or through consequential failures may lead to PTS. These events are the same as those used in recent risk studies. The first eight of these initiating events do not in themselves result in PTS, however, consequential events postulated as a result of these first eight initiators do result in transients with PTS. The events which could lead directly to a PTS challenge are small LOCAs, excessive feedwater, steamline rupture, and steam generator tube rupture. Consequential failures for these initiators can also enhance their seriousness as a cooldown challenge.

We believe that the WOG study has been sufficiently general and thorough to identify the event sequences of greatest significance to PTS risk. For the purposes of this study we have adopted the significant event sequences that they have identified in modified form after staff review. All of the significant event sequences have been characterized by the staff with respect to frequency per reactor year, the temperature time constant β, and the final reactor coolant system temperature at the pressure vessel wall. The staff review and evaluation of these event sequences included important changes to the initiating and consequential event frequencies based on the staff's PRA studies including Reference 6.2. Some changes in the temperature time constant and the final reactor coolant system temperature at the pressure vessel wall were also based on what we believe to be better thermal hydraulic analyses for some of the events considered. The event sequences determined by the WOG as reviewed and evaluated by the staff are further addressed in separate categories below.

6.2 Characterization of Specific Groups of Event Sequences Identified as Contributors to PTS Risk

As a result of the above more general approach to the problem of identifying event sequences reported in Reference 6.1, we believe that the events of significance to the PTS issue have been identified as secondary (steam side) depressurization, small-break loss-of-coolant accidents, and steam generator tube ruptures (special case of small-break LOCA). In order to characterize individual event sequences within each of these groups, certain additional parameters have been identified which determine the significance of these sequences as a PTS challenge.

The level of decay heat present during an initiating event is an important parameter in the cooldown from a given transient. The level of decay heat is related principally to the operational history (full power operation, hot zero power, other) immediately preceding the transient. The frequency of challenging event sequences are thus differentiated by the operational status of the plant.

The time allowed prior to initiation of proper operator action is another parameter that is important in some sequences. This variable has been used as a parameter in the results which characterize certain sequences that are presented below.

6.2.1 Secondary (steam side) Depressurizations

This group of cooldown events which involves some type of opening of the steam system includes steamline rupture of all sizes, inadvertent safety relief valve open to atmosphere, inadvertent steam dump valve open to the condenser, reactor trip without turbine trip, or operator error which results in any of these malfunctions. The transient is characterized by a rapid cooldown of the primary coolant system with shrinkage and consequential rapid depressurization until safety injection is actuated providing additional cooling and eventual primary repressurization. Natural circulation and, therefore, good mixing conditions are maintained in this transient for greater than 30 minutes unless low decay heat levels exist.

Parameters which are important with respect to severity of reactor coolant system cooldowns are (1) plant operational status (decay heat level), (2) operator action time to isolate auxiliary feedwater flow, (3) break size, (4) reactor coolant pump operation, and (5) location of the depressurization opening with respect to the main steam isolation valves.

Main Steam Line Break (MSLB)

An MSLB with break area larger than 6 inches equivalent diameter, results in a rapid cooldown of the primary system. The final temperature can be as low as around 200°F, depending on the plant operating status (decay heat level) and operator action to terminate auxiliary feedwater. The system will repressurize as a result of safety injection and may reach a pressure in excess of 2000 psig depending on when operator action is taken to terminate safety injection. The MSLB results in a signal to close the main steam isolation valves (MSIVs) so that only leaks upstream of the MSIV result in low final temperature. The event frequency, temperature time constant β, and final reactor coolant system temperature for the parameters of initial power and time for operator action to isolate feedwater are presented in Table 6.2.

The staff results presented in Table 6.2 show that the frequency of this event is significant to PTS risk whereas the frequency determined by the WOG study is extremely low. The staff's final reactor coolant system temperature for this transient is, however, much higher than the WOG result based on what we believe to be more realistic thermal hydraulic analyses for this transient.

Small Steam Line Break (SSLB) or Stuck Open Steam Generator Safety/Relief Valve

The SSLB or stuck open SG safety/relief valve can result in an overcooling transient similar to the MSLB but of longer duration due to the smaller break size. This event has a much higher frequency than the MSLB. The event frequency, temperature time constant β, and final reactor coolant system temperature for the parameters of initial power and time for operator action to isolate feedwater are presented in Table 6.3. The staff's results indicate a somewhat higher frequency for this event than the WOG study.

6.2.2 Small-Break Loss-of-Coolant Accident (SBLOCA)

The cooldown transient from an SBLOCA of the reactor coolant system includes reactor coolant pump seals, primary power-operated relief valve or safety valve failure or leakage as well as actual piping breaks of various sizes in hot or cold legs. For breaks less than a critical break size of about 1.5 inches, natural circulation is maintained and mixing occurs and the resulting cooldown rates are not expected to exceed Appendix G limits of less than 100°F per hour. Both the reactor coolant pump seal leak (break equivalent to 0.5 inches) and stuck-open power-operated relief valve (break equivalent to 1.4 inches) are included in that category.

Break sizes greater than 1.5 inches up to 6 inches are also included as SBLOCAs. For these breaks, the safety injection flow is less than the break flow, resulting in a net mass loss from the piping system. Loop flow (natural circulation) can be lost for this range of breaks, resulting in a rapid cooldown due to the cold safety injection flow. The exact break size where loss of flow occurs is dependent on the safety injection flow rate (and makeup flowrate), the break location, the decay heat level, and the SG (heat sink) performance. Because of the stagnation of flow, mixing of the safety injection water is poor and rapid cooldown of the vessel could result.

We have reviewed the frequency of events that may result in stagnated loop conditions such as SBLOCAs in the 2- to 6-inch equivalent diameter range. There are several small diameter pipes in the range of 2 to 4 inches connected to the main primary system piping. These include charging and letdown lines, RTD bypass lines, pressurizer spray lines, power-operated relief valve lines, and safety injection lines. SBLOCA events in the 2- to 6-inch range are dominated by non-isolatable breaks and, therefore, operator action is not a major parameter. The event frequency, temperature time constant β, and final reactor coolant system temperature are presented in Table 6.4.

As discussed above, these LOCAs can be differentiated by breaks smaller than about 2 inches where loop circulation is maintained (and good mixing of the cold safety injection water is therefore achieved) and breaks larger than about 2 inches where loop circulation is lost (and poor mixing of safety injection water results). The WOG judged LOCAs with effective diameters greater than 2 inches to have a negligible probability of causing vessel failure, so they only included LOCAs with breaks less than 1.5 inches. We agree that breaks in the size range less than 1.5 inches have small time constants and appear similar to slightly accelerated shutdown transients where the operator can be expected to control the pressure.

The significance of breaks in the 2- to 6-inch range to PTS risk has been separately analyzed. Fracture mechanics analyses performed with a more exact representation of this cooldown transient show that the PTS risk from this transient is less than could be anticipated and consistent with the screening criteria proposed (see Section 8).

6.2.3 Steam Generator Tube Rupture

The response of the reactor coolant system to a variety of steam generator tube failure events, up to the complete severance of a single tube, has been analyzed in Reference 6-1. These analyses simulate automatic protection systems, such as reactor trip and emergency core cooling systems, as well as operator actions. A steam generator tube failure should not result in a rapid cooldown of the primary or excessively high reactor coolant system pressures if current plant operating procedures are used. In general, natural circulation should develop in all primary loops and mix with incoming safety injection flow to preclude local temperature depressions if RCPs are stopped. However, the subsequent operator actions to terminate primary-to-secondary leakage may rapidly cool the reactor coolant system for short periods and may stagnate the faulted loop. In that case, local temperature depressions due to continued safety injection flow may occur. The period of this temperature depression is expected to be short and should not represent a significant PTS challenge to vessel integrity. The event frequency, temperature time constant β, and final reactor coolant system temperature are presented in Table 6.5. The frequency for this event estimated by the staff is significantly greater than that determined by the WOG.

6.3 Frequency of Low Probability Event Sequences Contributing to PTS Risk.

Tables 6.2, 6.3, 6.4, and 6.5 summarize the groups of postulated event sequences that appear to be significant PTS initiators in terms of their estimated expected frequency, and the parameters for a stylized transient

(T_f, β and P). Calculations also have been made of typical temperature and pressure behavior for each class of events. In principle, each of the sequences could be used to perform deterministic fracture mechanics calculations for a range of vessel RT_{NDT} values to determine the limiting value, RT_c, necessary to prevent crack initiation for each type of event, as was done for actually experienced events in Section 3.5 above. However, such calculations have not been made. Alternatively, the data of Tables 6.2, 6.3, 6.4, and 6.5 can be used to construct a cumulative frequency versus T_f distribution similar to that done for experienced events in Figure 2-14. This distribution is shown in Figure 6-1. In Section 3.5 above, it is shown that based on the deterministic fracture mechanics parametric studies, for relatively fast cooldown event ($\beta = 0.15\ min^{-1}$) with final temperatures in the 250-300°F range and high system pressures (~2300 psig), crack initiation (in longitudal welds) is not predicted if T_f is about 5-10°F higher than RT_{NDT}. Thus, the distribution curve of Figure 6-1 suggests that vessels with an RT_{NDT} of 270°F (the suggested screening criterion discussed in Section 4) would not be expected to experience longitudinal crack extension for events with frequencies greater than about 6×10^{-3} per reactor-year. This conclusion is similar to that obtained in Section 4 considering actual experienced events.

However, the frequency distribution of Figure 6-1 extends to lower probability events with low values of T_f. This low frequency "tail" on the distribution indicates that there are postulated events with estimated frequencies as high as 10^{-4} per reactor year for which the final temperature is substantially below 270°F that must be considered. This is discussed in Section 8.

The discussion above is subject to the same large uncertainties as are described in Section 4 above. To gain additional insights into the conservatisms in the deterministic fracture mechanics treatment and to gain some notion of the risk of vessel failure considering low probability events, the data of Tables 6.2, 6.3, 6.4, and 6.5 are used in combination with a probabilistic treatment of fracture mechanics described in Section 7. The results are discussed in Section 8.

REFERENCES - SECTION 6

6.1 "Summary of Evaluations Related to Reactor Vessel Integrity performed for the Westinghouse Owners Group," Westinghouse Electric Corporation, Nuclear Technology Division, May 1982.

6.2 "Reactor Safety Study, An Assessment of Accident Risks in U.S. Commercial Nuclear Power Plants," WASH-1400 (NUREG-75/014) October 1975.

Table 6.1 Initiating Events

1. Loss of Main Feedwater (LOFW)
2. Closure of One Main Steam Isolation Valve (MSIV)
3. Loss of Primary Flow (LOPF)
4. Core Power Increase (POWIN)
5. Turbine Trip (TT)
6. Spurious Safety Injection Activation (SSI)
7. Reactor Trip (RT)
8. Turbine Trip Due to Loss of Offsite Power (TT/Loop)
9. Steam Generator Tube Rupture (SGTR)
10. Small LOCA, <1.5 inch Diameter (LOCA-1)
11. Small LOCA, 1.5 inch Diameter (LOCA-2)
12. Large LOCA, 6 inch Diameter (LOCA-3)
13. Excessive Main Feedwater (EX FW)
14. Steamline Rupture Inside Containment (STM BRK In)
15. Steam Rupture Outside Containment (STM BRK OUT)

Table 6.2 Event parameters for the main steam line break (MSLB)

Plant Status	Hot Full Power				Hot Zero Power			
Operator Isolates Auxiliary Feed-water min.	0-5	5-10	10-30	30-60	0-5	5-10	10-30	30-60
Event Frequency per Reactor-Year	8×10^{-5}	6×10^{-5}	1.5×10^{-5}	$3. \times 10^{-7}$	8.5×10^{-6}	6.8×10^{-6}	1.6×10^{-6}	$3. \times 10^{-8}$
Temperature Time Constant, β min^{-1}	0.4	0.2	0.09	0.09	0.4	0.2	0.2	0.2
Final Reactor Coolant System Temperature at Vessel Wall, °F	450	390	300	250	212	212	210	190

Table 6.3 Event parameters for the small steam line break (SSLB)

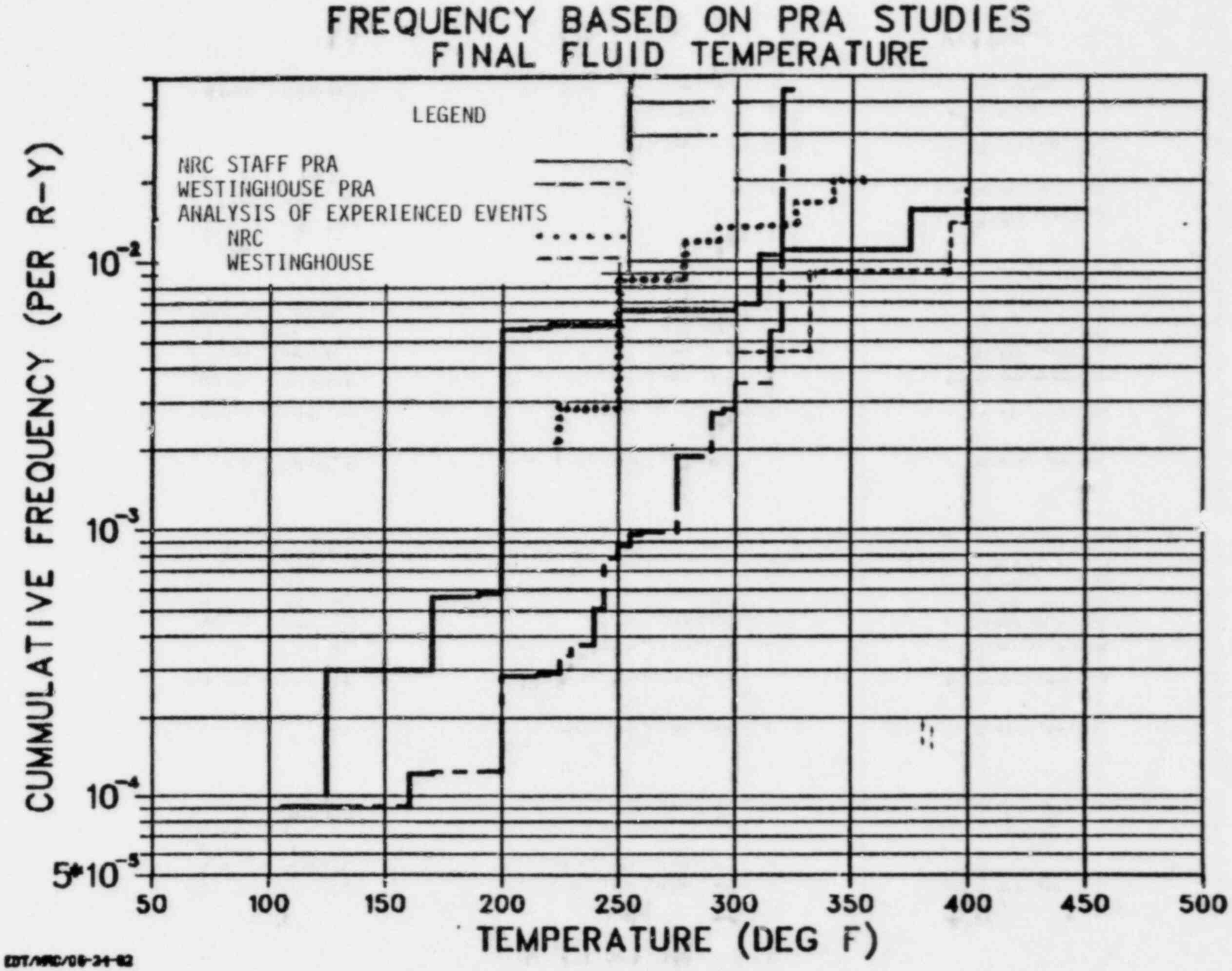
Plant Status	Hot Full Power					Hot Zero Power				
Operator Isolates Auxiliary Feedwater, Min.	0-5	5-10	10-20	20-30	30-60	0-5	5-10	10-20	20-30	30-60
Event Frequency Per Reactor-Year	4.5×10^{-3}	3.6×10^{-3}	8×10^{-4}	6.3×10^{-5}	1.8×10^{-5}	4.5×10^{-4}	3.6×10^{-4}	8×10^{-5}	6.3×10^{-6}	1.8x10
Temperature Time Constant, β, min^{-1}	0.4	0.2	0.1	0.06	0.06	0.4	0.2	0.1	0.06	0.06
Final Reactor Coolant System Temperature At Vessel Wall, °F	385	320	250	220	200	375	310	235	200	175

Table 6.4 Event Parameters for the Small-Break Loss-of-Coolant Accident (SBLOCA), Break Size 2-6 inches Equivalent Diameter

Event Frequency Per Reactor-Year	3×10^{-4}
Temperature Time Constant, β, min^{-1}	0.12
Final Reactor Coolant System Temperature At Vessel Wall, °F	125

Table 6.5 Event parameters for steam generator tube rupture

Without Steam Line Break or Stuck-Open SRV		
Event Frequency Per Reactor-Year		5×10^{-3}
Temperature Time Constant, β, min^{-1}		0.04
Final Reactor Coolant System Temperature At Vessel Wall, °F		125
With Steam Line Break or Stuck-Open SRV		
	Outside Containment	Inside Containment
Event Frequency Per Reactor-Year	2.5×10^{-4}	1×10^{-5}
Temperature Time Constant, β $min.^{-1}$	0.04	0.04
Final Reactor Coolant System Temperature At Vessel Wall, °F	170	170



FREQUENCY BASED ON PRA STUDIES
FINAL FLUID TEMPERATURE
LEGEND
NRC STAFF PRA
WESTINGHOUSE PRA
ANALYSIS OF EXPERIENCED EVENTS
NRC
WESTINGHOUSE
CUMMULATIVE FREQUENCY (PER R-Y)
10^{-2}
10^{-3}
10^{-4}
$5*10^{-5}$
50
100
150
200
250
300
350
400
450
500
TEMPERATURE (DEG F)

FIGURE 6-1

7. PROBABILISTIC TREATMENT OF FRACTURE MECHANICS

7.1 Introduction

The deterministic fracture mechanics analyses discussed in Section 3 assume specific values for all the input parameters necessary to predict crack initiation, growth and/or arrest. However, many of these parameters are not known precisely. In order to quantitatively analyze the effect of a large number of uncertainties, a probabilistic approach can be taken to estimate the failure probability of a reactor pressure vessel. A Vessel Integrity Simulation Analysis (VISA) code was developed to gain insight into reactor pressure vessel failure probability due to pressurized thermal shock. Appendix H discusses in detail the probabilistic fracture mechanics analysis and the VISA code. A brief description of the VISA code is presented in Section 7.2.

7.2 Description of VISA Code

The VISA code consists basically of two parts. The first is a deterministic fracture mechanics analysis for a specified pressure/temperature transient. This analysis is similar to that discussed in detail in Appendix D and includes heat transfer, thermal and pressure stress, and applied stress intensity value calculations for a range of crack depths. The second part uses Monte Carlo techniques to assess failure probability based on a very large number of deterministic calculations in which the input parameters are varied.

Certain parameters are treated as random variables, and their values are sampled from a statistical distribution defined as an input to the program. In each calculation, values of the random variables (crack depth, copper content, initial RT_{NDT}, fluence, critical stress intensity factor, K_{Ic}, and stress intensity factor for crack arrest, K_{Ia}) are selected from the specified probability distributions, and deterministic calculations are made using these values. Each calculation results in one of three outcomes: (1) no crack initiation, (2) crack initiation followed by arrest, or (3) pressure vessel failure.

For each iteration of the simulation, values of fluence, flaw size, and copper content are selected from their respective distributions. The RT_{NDT} at the inner wall is calculated as a function of fluence and copper content. With these values fixed for the iteration, the code steps through the time history of the transient. For each time step, the stress intensity at the crack depth is taken from the deterministic portion of the code. A value of K_{Ic} is simulated to determine fracture initiation. If initiation does not occur, the simulation moves to the next time step. If initiation does occur, the crack is extended 1/4 in., and the crack arrest toughness (K_{Ia}) is simulated. If arrest occurs, the simulation moves to the next time step; if not, the crack is extended another 1/4 in. and a new value of K_{Ia} is simulated. This process ~~is~~ continued until either the vessel fails or the duration of the transient is reached. Each pass through the simulation loop represents a single computer calculation conducted to determine if RPV failure will occur. Up to a million passes through this loop can be made. The code keeps track of the number of crack initiations and RPV failures. The probabilities of crack initiation and RPV failure then are estimated by dividing these values by the total number of trials. Thus, the VISA code actually performs millions of deterministic calculations with each set of calculations based on a different set of values selected from the appropriate statistical distributions for the significant variables. This is the calculational equivalent to subjecting a population of up to a million operating reactor pressure vessels to the pressurized thermal shock transient of interest and then inferring the failure probability based on the number of observed failures.

7.3 Probabilistic Fracture Mechanics Sensitivity Studies

Section 3 and Appendix D of this report discuss the sensitivity of crack initiation and vessel failure to the various PTS parameters. This section discusses the same sensitivities based on probabilistic fracture mechanics. Results are portrayed in Figures 7-1, 7-2, 7-3 and 7-4 for the stylized thermal transient discussed in Section 2 above:

$$T_w = T_f + (To - T_f)e^{-\beta t}$$

Figure 7-1 illustrates the sensitivity of the conditional vessel failure probability to T_f-RT_{NDT}, for an assumed β of 0.15 reciprocal minutes and a pressure of 1000 psig. It is seen that the relative risk is low for RT_{NDT} less than T_f, but if cooldown drops temperature below the vessel RT_{NDT}, then the risk rises quite rapidly.

Figure 7-2 illustrates the sensitivity of conditional failure probability to pressure. The case shown is for a T_f-RT_{NDT} of 50°F and a β of 0.15 reciprocal minutes.

Figure 7-3 illustrates the sensitivity to the decay parameter, β, with the other parameters held constant.

Figure 7-4 illustrates the sensitivity of two postulated transients to the heat transfer coefficient used. For the relatively low heat transfer coefficients at low flow conditions, the risk is quite sensitive to the value (or correlation) used.

Appendix H includes more information regarding the sensitivity of relative failure probability to parametric assumptions. Although these studies assumed somewhat different input assumptions regarding the relation of RT_{NDT} to fluence and fluence attenuation through the wall than were used for the deterministic fracture mechanics studies (Section 3 and Appendix D), the same trends are found.

These probabilistic sensitivity studies do not include the effect of cladding stresses. Based on the conclusions stated in Section 3, it is estimated that inclusion of the cladding stresses would shift the curve of Figure 7-1 approximately 10° to the right, thus increasing the risk about a factor of 2 or 3 for that assumed transient.

Because the probabilistic fracture mechanics studies were conducted for only a limited range of parameters, the results should not be extended beyond these ranges. For instance, if T_f was only a few tens of degrees below 550°F, the thermal shock to the vessel is significantly less severe than say for a cooldown to 200°F or lower. Thus, in terms of probability versus T_f-RT_{NDT}, the results are expected to be considerably different.

The technology regarding probabilistic fracture mechanics as related to PTS scenarios have evolved only during the past few years and perhaps is still some way from reaching maturity. It is, however, believed to be a useful tool. The NRC plans to develop the technology further, and the industry is encouraged to do the same. Future work is expected to include consideration of warm prestressing effects for a variety of postulated transients, the effect of cladding and perhaps other crack shapes.

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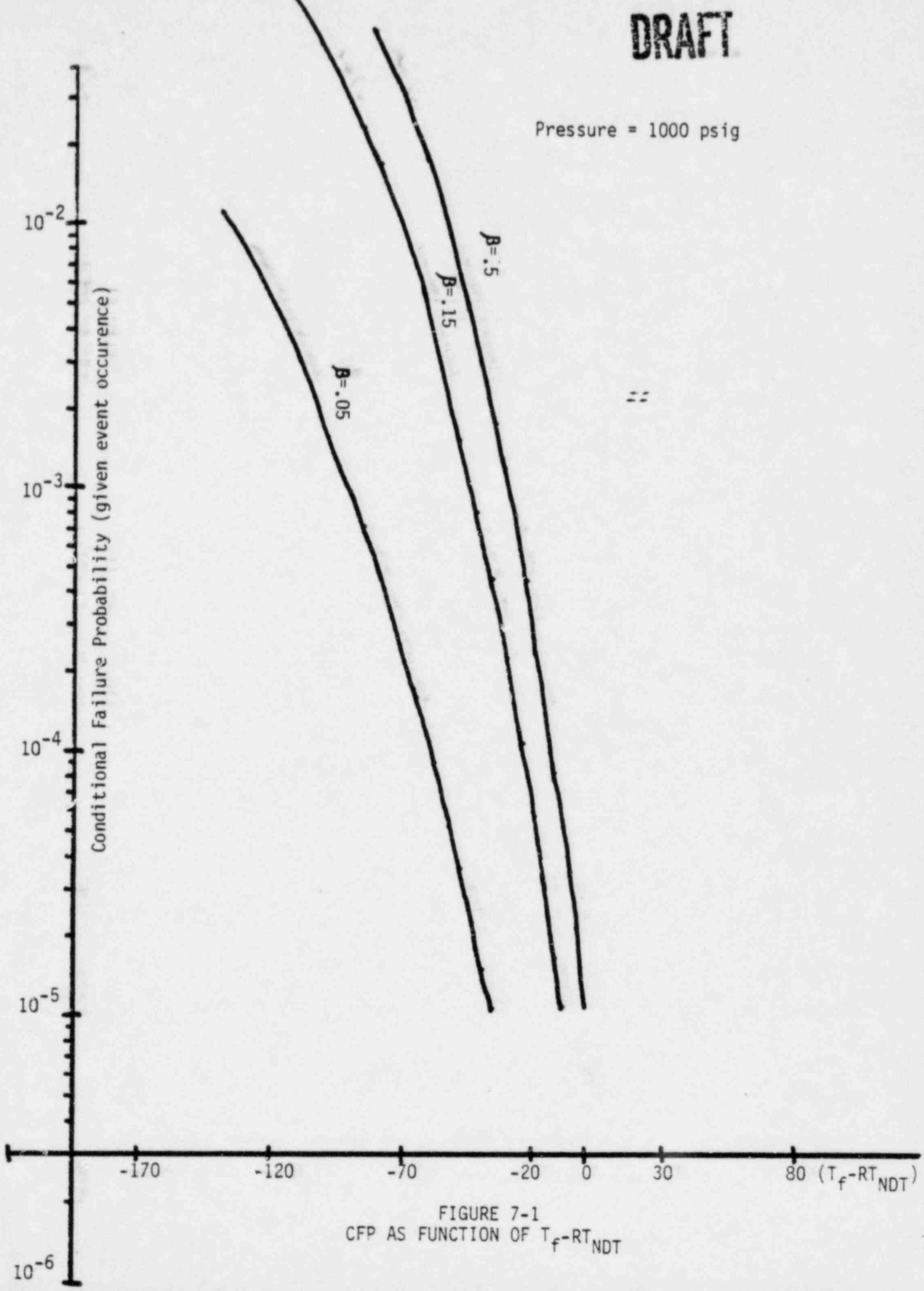

FIGURE 7-1
CFP AS FUNCTION OF T_f-RT_{NDT}

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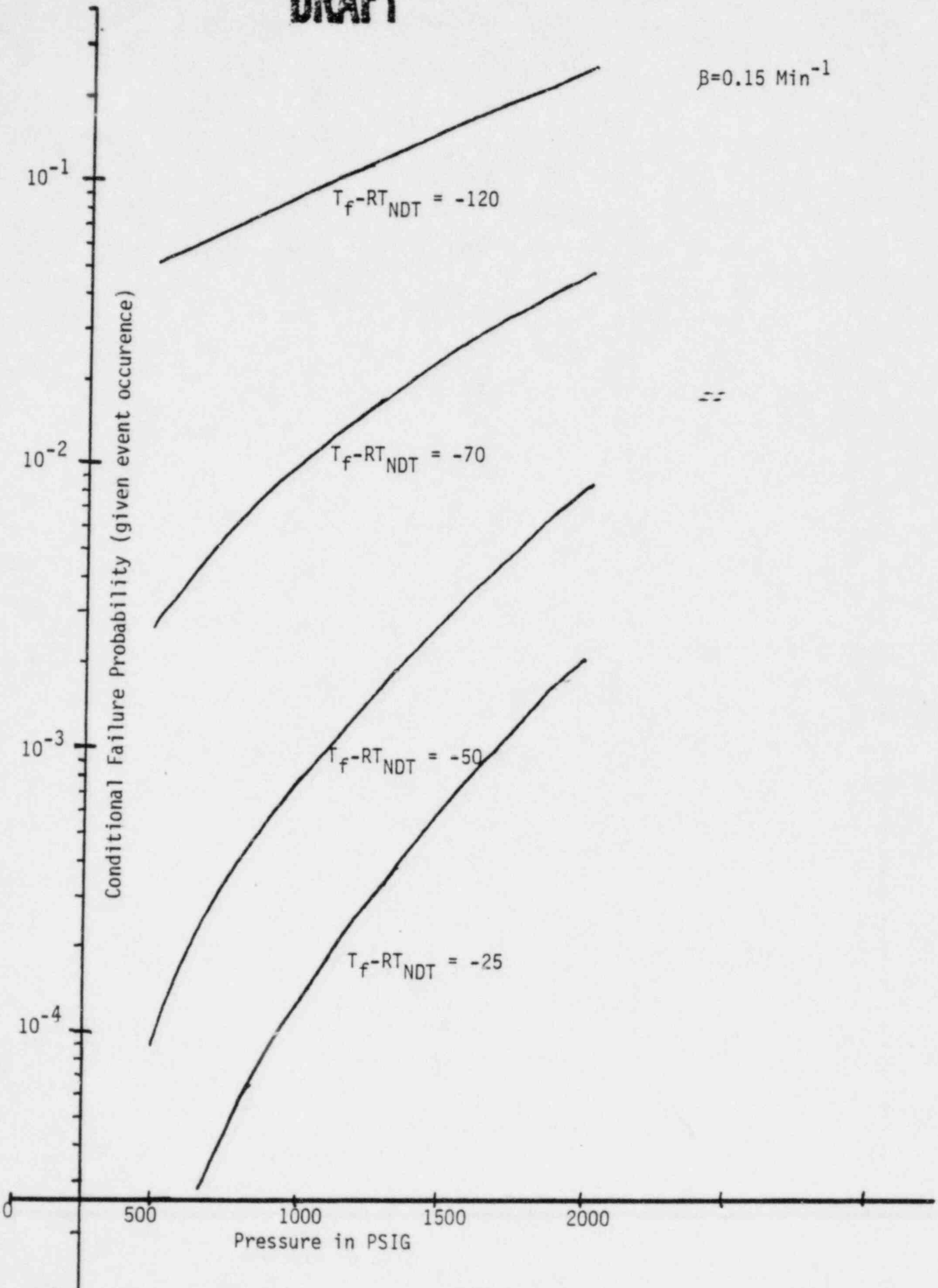

FIGURE 7-2
CFP AS FUNCTION OF PRESSURE

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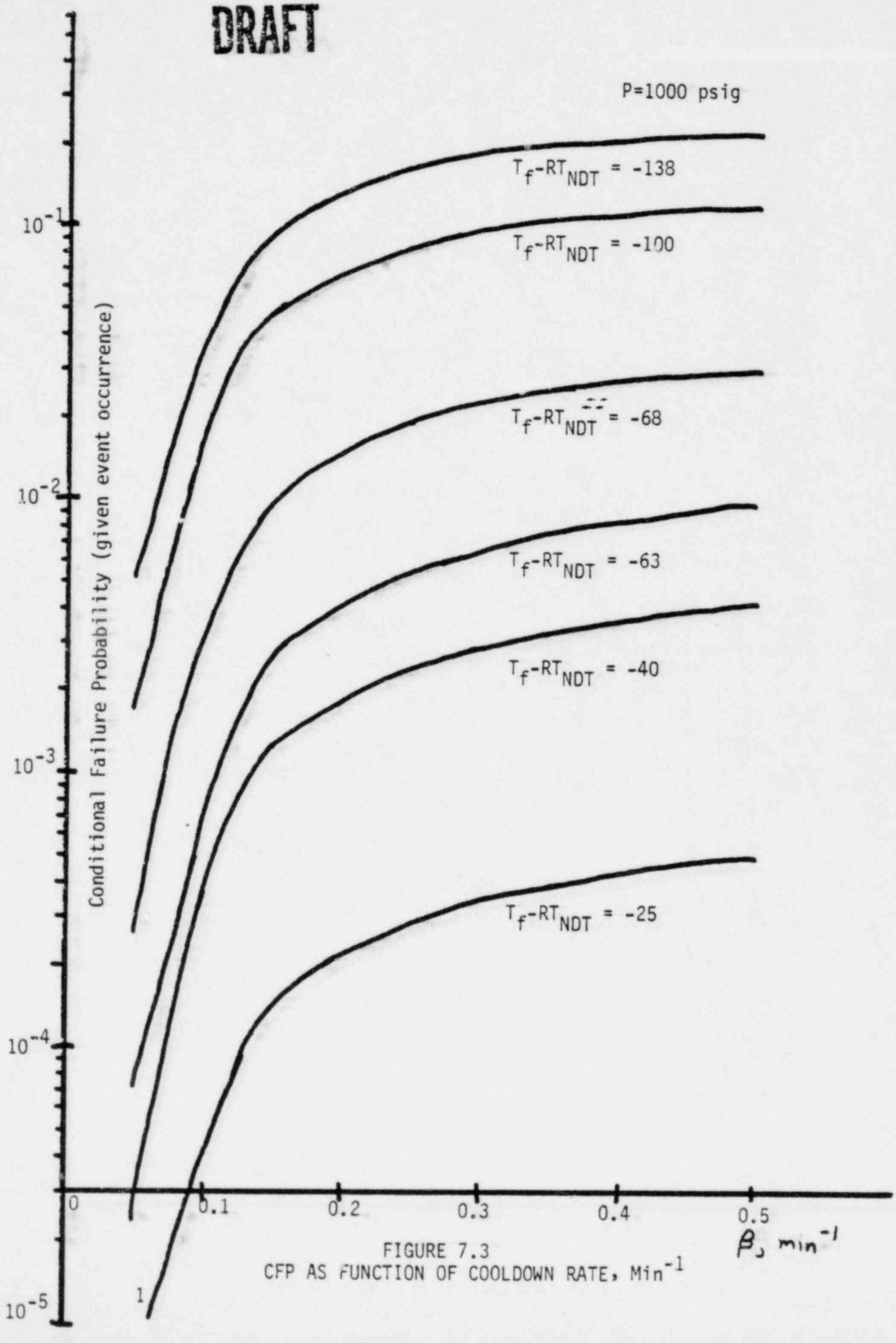

FIGURE 7.3
CFP AS FUNCTION OF COOLDOWN RATE, Min^{-1}

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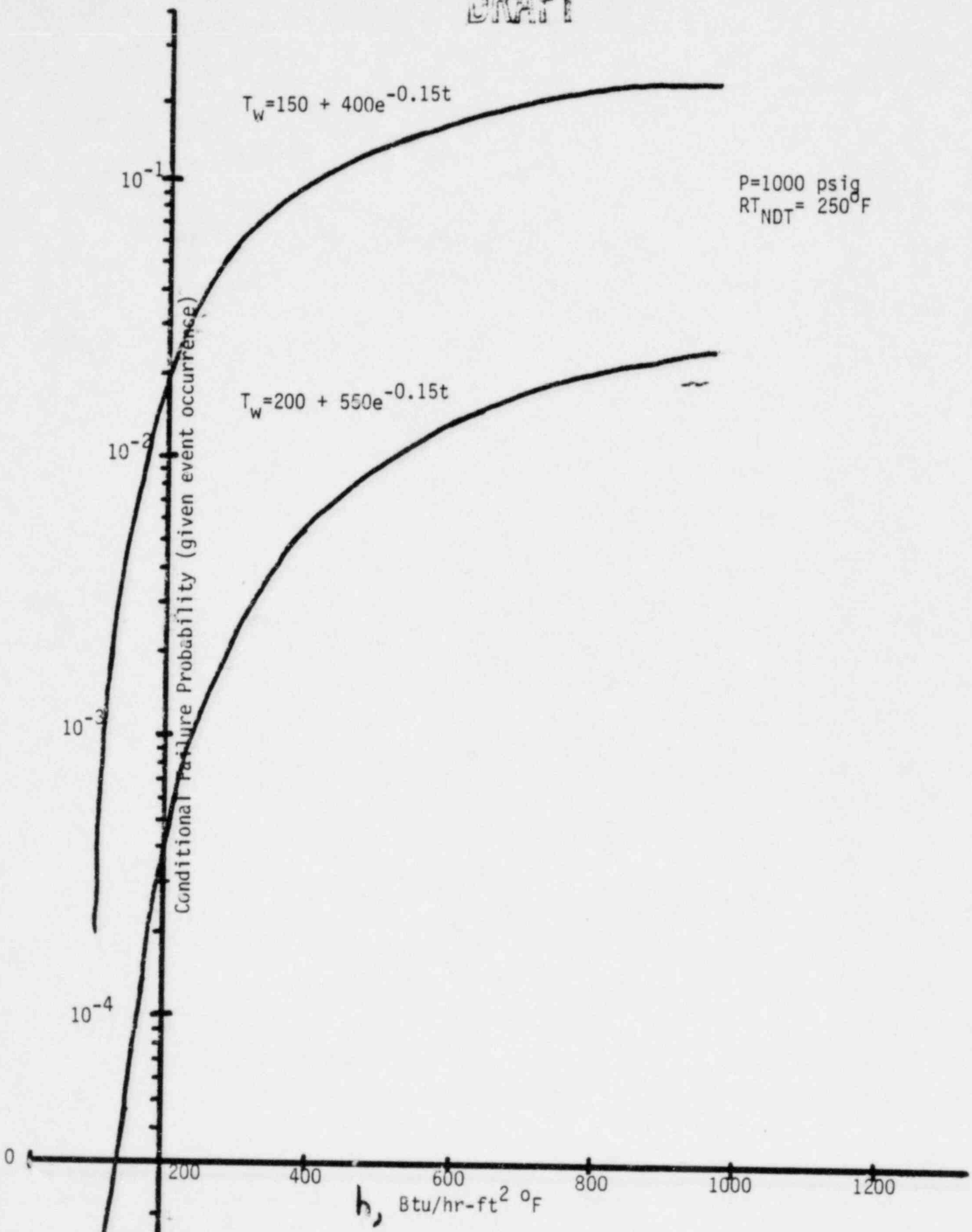

FIGURE 7.4
CFP AS FUNCTION OF HEAT TRANSFER COEFFICIENT

8. PROBABILITY OF VESSEL FAILURE

8.1 Introduction

This Section summarizes a probabilistic study of reactor pressure vessel (RPV) failure as a result of pressurized thermal shock (PTS). The calculational method combines the frequencies of overcooling transient sequences (Sections 2 and 6) with the probabilistic treatment of RPV failure (Section 7). The results are expressed in terms of the probability, per reactor-year, of RPV failure due to PTS. Some risk considerations are also considered, and also the relationship of PTS to the current regulations.

8.2 Methodology

The basic approach is essentially a combination of (1) a probabilistic analysis of overcooling transients, plus (2) a probabilistic analysis of the consequences of such transients to the RPV, and the probability of RPV failure, given the transients.

In order for this procedure to be valid, the transient sequences must be separated, and analyzed in groups with similar properties. The course and severity of each transient group can then be used as the input transient for analysis of RPV behavior. In the work reported here, the transient sequences were obtained from calculations furnished by the Westinghouse Owners Group (Ref. 8.1), and also from transient analyses based on the WOG analysis but revised by the NRC staff as described in Section 6 and Appendix G.

The transient groups were derived from consideration of the various possible sequences following each of the initiating events--excess feedwater, small-break LOCA-etc.--given in Appendix G. The analyses of the frequencies and courses of the different sequences are also reviewed in Appendix G.

The VISA code, in its present state of development, can accept only stylized input transients characterized by T_f, β, and P. Therefore, each transient group was so stylized. Cumulative frequency distributions of the T_f values used in this study are given in Fig. 8-1. The number of groups used was 86 for the WOG distribution and 23 for the staff-modified distribution.

For each transient group, the values of T_f, β, and P were used, with the VISA code, to establish a probability of RPV failure, given the occurrence of a transient with the specified characteristics. Multiplication of this conditional RPV failure probability by the frequency of occurrence of the transients comprising the group then gives the frequency of RPV failure caused by this group of transients.

The sum of these failure frequencies gives the RPV failure frequency (per reactor-year) as caused by the ensemble of transients of all the groups considered.

8.3 Accuracy and Completeness

In order for the RPV failure frequency so calculated to be correct, the ensemble of transients must be complete. That is, all transient sequences capable of inducing RPV failure due to PTS must be included.

The WOG analysis included consideration of several hundred candidate sequences, not all of which turned out to be PTS precursers. The array of initiating events and event sequences is given in Ref. 8.1 and summarized in Appendix G. Variations in reactor power level, leak size (for LOCA and steamline break sequences), and operator actions were included. The staff review concentrated on the transient groups shown in the WOG analysis to be dominant, but also considered other candidates not significant in the WOG analysis; see again Appendix G. In all, some hundreds of possible sequences were reviewed by WOG, staff, or both.

Like all probabilistic analyses based on event sequences, the probabilistic PTS analysis presented here cannot be proved complete. The differences between the

WOG and staff anlayses shcw that more work can and should be done to investigate additional candidate sequences and to validate some of the approximate sequence analyses. However, the work done to date suggests that the principal contributors have probably been identifed as well as can be done in approximate, generic analyses. Improved approaches to completeness should be sought in connection with the plant-specific analyses we recommend for plants soon to exceed the screening criterion (Sections 4 and 9).

Completeness aside, the accuracy from both the transient sequences and the vessel calculations, are subject to substantial uncertainties. In particular, the probabilistic treatment of fracture mechanics (Section 7, Appendix H) is still under development. Both the methodology and the probability distribution functions used as input information are sources of variation in the results. Detailed study of these variations has not yet been accomplished. Moreover, much more extensive sensitivity studies are planned. Therefore, the numerical results given in this Section must be taken for what they are worth. Rather than close estimates of absolute RPV failure probability, the calculated values should be used for insight into trends and sensitivities. The values of the calculated probabilities of failure for a given set of nominal conditions is believed by the researchers (Appendix H) to be uncertain by plus or minus at least two orders of magnitude, a broad band of uncertainty, indeed. Also, the steepness of the curves (Appendix H) shows a high sensitivity of the result (calculated RPV failure probability) to variations in the values of T_f, β and P assigned to the transient group. The calculation of these quantities is approximate, even for a well defined event sequence. The lesson from transients actually experienced (Section 2, Appendix G) is that real transients don't look like exponentially decaying temperatures with constant pressures. Thus another source of uncertainty is introduced by the stylized transients necessarily used in this calculation, at the present state of the art.

8.4 Results

With due consideration of the uncertainties discused just above, we present the results of the probabilistic PTS calculations in Figs. 8-2 and 8-3. The details are given in Appendices G and H.

Figures 8-2 and 8-3 show, as a function of RPV embrittlement, the expected frequency of RPV failure due to PTS. The abcissas are the reference temperatures, RT_{NDT}, at the inner surface of a RPV having the mean values of RT (0), neutron fluence, and copper content of the probability distribution functions used for these parameters. The ordinate is the failure frequency of the RPV so characterized, per reactor-year, owing to the PTS transient subclasses (LOCA, SLB, etc.) as labelled, and the total RPV failure frequency due to PTS. New vessels start at the left side of these diagrams, with very low RT_{NDT} and negligibly small PTS probability. As the vessels are irradiated, their characteristics move to the right, and an increasing number of increasingly probable overcooling transients have increasingly high probability of inducing RPV failure.

Figure 8-2 gives the results for the WOG distribution of transients; Figure 8-3, the NRC staff distribution.

The steepness of the curves in Figures 8-2 and 8-3 shows a high sensitivity of RPV failure probability to the value of RT_{NDT} (as defined for these curves). A change in RT_{NDT} as small as 20-30°F changes the calculated probability by a factor of 10, on some of these curves. Yet we know neither the actual value of RT_{NDT} for a given RPV, nor the severity of a given transient, to within this order of accuracy. This is another way of restating the substantial uncertainties in the present state-of-the-art of making analyses of this kind. For this reason, the NRC staff recommends that the PTS criteria--screening or otherwise--should not be determined by where these curves cross some acceptable value of risk. Rather, the probabilistic curves mean to us that a substantial margin to failure exists for vessels approaching the screening criterion.

8.5 Relationship to Safety Goal

In February 1982, the Commission published for comment a "Proposed Policy Statement on Safety Goals for Nuclear Power Plants" (Ref. 8.2). Although the Safety Goal guidelines have not been adopted (at least not yet), it is instructive to compare the proposed PTS requirements to the guidelines.

Core Melt. - The core melt Safety Goal guideline states, "The likelihood of a nuclear reactor accident that results in a large-scale core melt should normally be less than one in 10,000 per year of reactor operation." This suggests that the core melt frequency ascribable to one sequence, for example PTS, should not exceed approximately 10^{-5} per reactor-year.

Because of the unusually large uncertainty in the risk estimation for PTS, compared to other sequences, a value of less than 10^{-5} might well be assigned for a safety goal for PTS. We have not done this in the discussion in this section, but have used 10^{-5}. The reader should keep in mind that the risk numbers for PTS given in the following discussion are highly uncertain.

We have no technical analysis of the course and consequences of a PTS sequence that involves RPV failure. Determination of the RPV failure mode (better, estimation of the probabilities of the various failure modes) has not been done and is dependent on the details of the scenario. Moreover, the outcome would likely be dependent also on the plant design details. In particular, ice condenser containments would be expected to have different failure modes, with different probabilitics, than large dry containments.

The breach in the RPV would be a LOCA, which might or might not prevent ECCS effectiveness. A large through-wall crack would probably lead to core melt. Axial cracks and most circumferential cracks would not likely lead to early containment failure; the massive concrete shielding would intercept missiles and the containment could stand the temperature and pressure. (Again, ice condensers have not been evaluated.) Whether a complete circumferential failure (which seems low in probability) would lead to large RPV (and core) motions is not well known.

The result of such approximate and intuitive analysis is that not all PTS failure events lead to core melt, but the fraction that do has not been analyzed quantitatively.

Public Risk. - The Draft Policy Statement includes quantitative guidelines for risk to individual members of the public, and for society at large, from reactor accidents. For analyzing how PTS events contribute significantly to the risk to the public, the following logic applies:

1. PTS event sequences leading to RPV failure have overall frequency F per reactor-year. Figures 8-2 and 8-3 provide a very approximate estimate of F. A plant evaluated (as described in Section 5 or 9 and Appendix E) to be at the 270°F screening criterion is likely to have a true RT_{NDT} of 150-270°F (two sigma ≅ 60°F). For the mean of 210°F, F ≅ 10^{-6} per reactor-year on the NRC curve (Figure 8-3), and much smaller on the WOG curve (Figure 8-2).

2. A fraction X<1 of RPV failure sequences leads to core melt, giving an expected value of XF core melts per reactor year.

3. A fraction Y of failure leading to core melt leads to significant radioactive release, so the expected value of the frequency of significant releases due to PTS is XYF.

4. To show PTS risk to be lower than 10% of the safety goal guidelines would involve showing

$$X F \lesssim 10^{-5} \text{ per reactor-year}$$

and

$$XYF \lesssim 10^{-8} \text{ per reactor-year}$$

We have only approximate values for F, and no quantitative values for X or Y. If XF is about equal to 10^{-5}, then Y would have to be no greater than 5×10^{-3}; that is, only one core melt in 200 should lead to lethal releases. Thus, for Y $\leq 5 \times 10^{-3}$, the core melt guideline will dominate.

The results of the probabilistic analysis given in item 1 just above, show F $\leq$ 5×10^{-6} per reactor-year, so that (even for X = 1, which is unrealistically high) Y $\leq 5 \times 10^{-2}$ is sufficient to show the risk to be within the guideline.

ALARA. - The Draft Policy Statement gives a cost-benefit guideline for decisionmaking of $1000 per man-rem averted.

For scenarios involving core melt without significant releases, the core melt guideline will govern and ALARA is not a consideration.

For early containment failure scenarios, as much as 50×10^6 man-rem might be involved, at a frequency of XYF. The expected value of the exposure would, therefore, be 50×10^6 XYF. For XYF $\lesssim 5 \times 10^{-8}$, the expected value would be less than 2.5 man-rem, and the ALARA guideline would not be a consideration for these sequences, either.

In summary, comparison of the approximate probabilistic PTS analysis reported here with the Draft Policy Statement on Safety Goals shows satisfactory conformance with the proposed screening criterion.

8.6 Relationship to Licensing Criteria

The regulation most directly applicable to PTS is 10 CFR 50, Appendix G. Paragraph IV.A.2.c states:

> Whenever the core is critical, the metal temperature of the reactor vessel shall be high enough to provide an adequate margin of protection against fracture, taking into account such factors as the potential for overstress and thermal shock during anticipated operational occurrences in the control of reactivity. In no case when the core is critical (other than for the purpose of low-level physics tests) shall the temperature of the reactor vessel be less than the minimum permissible temperature for the inservice system hydrostatic pressure test nor less than 40°F above that temperature required by section IV.A.2.a.

The Appendix G procedure used for determining "an adequate margin of protection" includes the postulation of a reference semi-elliptical surface flaw having a depth of 1/4 of the section thickness with a length six time its depth. In addition, the stress intensity factor due to pressure is increased by a factor of two. Because pressure stresses dominate for hydro testing and normal startup and shutdown situations, the reactor vessel integrity is not jeopardized if Appendix G requirements are met.

For severe cooldown transients, however, thermal stresses near the inner vessel surface are relatively high and dominate. The material toughness is also lower near the surface than deeper into the wall because of the lower temperatures near the surface. Hence consideration must be given to relatively shallow flaws. Thus procedures different from but equivalent to those of Appendix G are necessary to provide an adequate margin of protection.

The staff's present view is that the proposed PTS requirements may well require that Appendix G to 10 CFR 50 be amended, or supplemented.

Another potential regulation interface is 10 CFR 50.46 and 10 CFR 50, Appendix K. While the thrust of these regulations is to cooling effectiveness, paragraph (b)(5) of 10 CFR 50.46 requires,

> After any calculated successful initial operation of the ECCS, the calculated core temperature shall be maintained at an acceptably low value and decay heat shall be removed for the extended period of time required by the long-lived radioactivity remaining in the core.

If "successful initial operation" involves a PTS scenario, as can happen for 2 to 6 inch breaks (Sections 2 and 6, Appendix G), then "long-term cooling" can be jeopardized.

This scenario is discussed in Section 6. Since this sequence is calculated to dominate the risk, it was the subject of detailed examination, as discussed in Section 6.2.2. For this sequence, detailed calculations of system response were used, rather than the stylized T_f, β, and P. The WOG calculations, which we accept, include fluid mixing in the cold leg as predicted from experimental results, heat input from hot piping walls, and an assumed temperature of 60°F for the injected coolant. These calculations used the NRC assumptions for crack arrest, but should be corrected by -10°F to allow for cladding effect (see Section 3). The result is a predicted critical RT_{NDT} of approximately 270°F, consistent with the proposed screening criterion.

We conclude that a small break LOCA in a vessel within the proposed screening criterion has an acceptably low probability of vessel failure, so 10 CFR 50.46 is not infringed by the proposed requirements.

8.7 Conservatisms and Non-Conservatisms

The calculations summarized in Sections 3, 5, 7 and 8 and described in detail in the appendices and references, contain uncertainties of various sorts. The following paragraphs briefly summarize the most significant sources of uncertainty.

Operating Experience - The three most severe events took place in B&W plants. We have neglected, for lack of sufficient data to do otherwise, plant design differences in evaluating the experience. We have also neglected all the actions taken since TMI, Rancho Seco, and Crystal River to improve design and operations and thereby make these transient sequences less likely in the future. These are substantiated conservatisms in the inference from operating experience.

The temperatures used to characterize operating experience were measured in cold legs. The fluid in the downcomer could have been warmer (from mixing) or colder (from stratification) than the measurements.

Operation Actions - The analyses include the probability of the operating staff failing or delaying performing a needed operation, but do not include either successful mitigating actions or wrong actions that could make the events more severe.

Flaws and Cracks - The deterministic calculations assume the presence of a long through-clad flaw of critical depth--a substantial conservatism. The probabilisitc calculations use a through-clad crack probability many people believe is conservative. No account is taken of actual in-service inspection results in these generic calculations.

The crack growth/arrest model used by the staff assumes long initial flaws that grow uniformly over their length. This initial flaw shape is conservative. The growth/arrest shape is discussed in Section 3; we believe that, once a crack initiates, the long crack is a more realistic description than less conservative shapes used in other models.

Stresses - The models include no residual stresses, which is non-conservative. The NRC model includes cladding effect, which is realistic for through-clad cracks.

None of the models currently includes a warm prestress (WPS), which is a conservatism for transients satisfying the WPS conditions.

Material Properties - The estimation of RT_{NDT} at the 2 sigma condifence level is a substantial conservatism; see Sections 5.4 and 8.5.

Fracture Mechanics - The use of linear elastic fracture mechanics in the temperature region around RT_{NDT} is believed by many people to be conservative, since considerable ductility exists. Until we have validated applicable elastic-plastic models, however, the degree of conservatism cannot be determined.

Uncertainties in Probabilistic Calculations - Substantial uncertainties exist in probabilistic calculations as discussed in Section 8.3. The characterization of event sequences by T_f, β, and P is an oversimplification that is usually on the conservative side.

The net result of the above considerations is that the PTS analyses have substantial uncertainty, and are on balance substantially conservative. Neither the uncertainty nor the conservatism has been quantified.

A plant with a value of RT_{NDT} conservatively established in accordance with the NRC staff prescription of Section 5, equal to the screening criterion of 270°F, will have a risk from PTS consistent with, or below, the Safety Goal Guidelines.

Plants with higher RT_{NDT} would be predicted to have higher PTS risk, so the additional evaluations and requirements of Sections 9 and 10 are proposed.

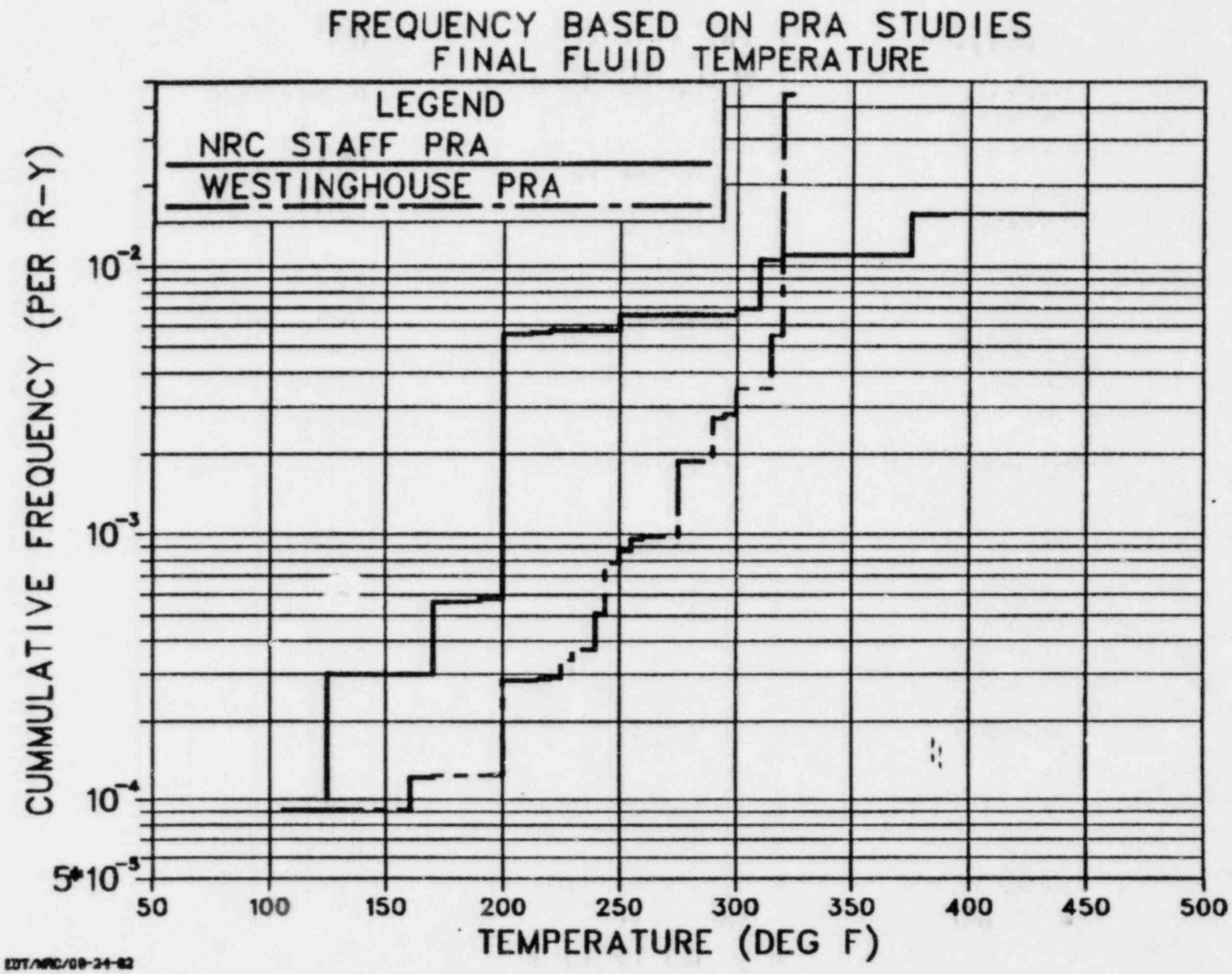

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FIGURE 8-1

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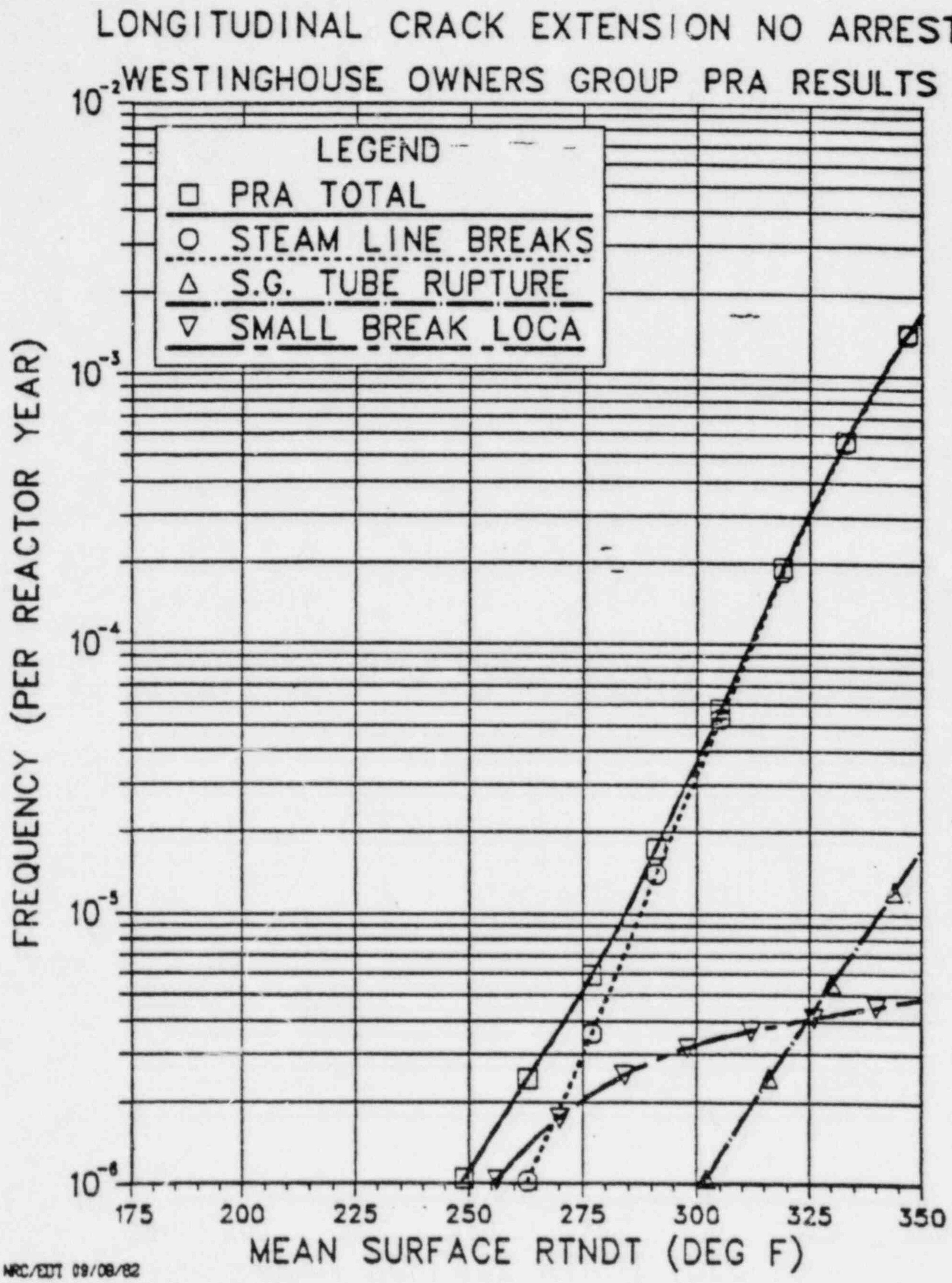

FIGURE 8-2

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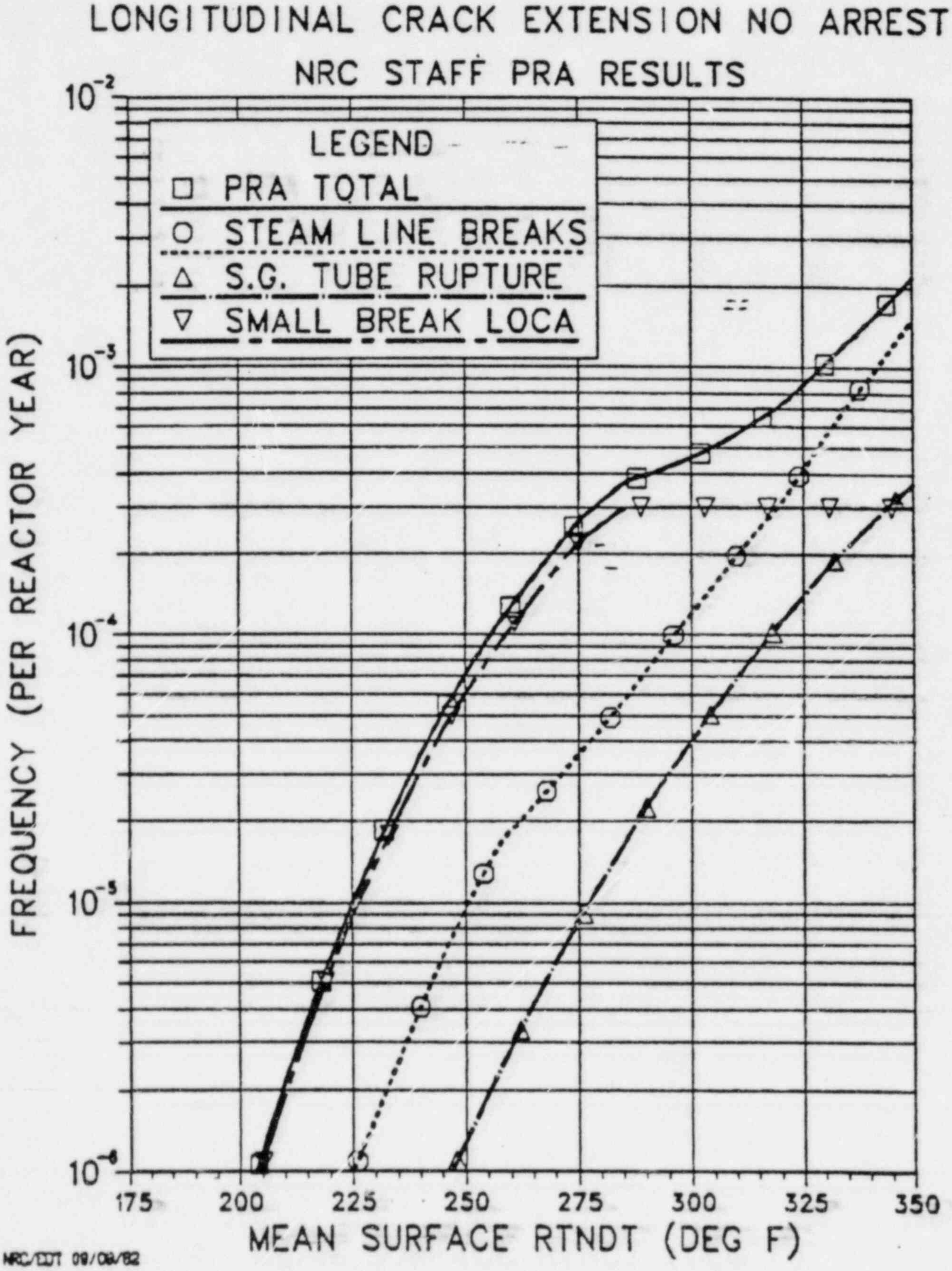

FIGURE 8-3

References

8.1 Letters dated May 28, 1982 (OG-70) and July 15, 1982 (OG-73) from O.D. Kingsley, Chairman, Westinghouse Owners Group to H.R. Denton, NRC; and additional probabilistic PTS results transmitted informally on June 22, 1982.

8.2 "Safety Goals for Nuclear Power Plants: A Discussion Paper," NUREG-0880, February, 1982. (The draft policy statement is quoted on pp vii-xxi.)

9. PLANT-SPECIFIC ANALYSES AND EVALUATIONS TO BE PROVIDED BEFORE THE SCREENING CRITERION IS EXCEEDED

9.1 Introduction

The study of pressurized thermal shock to determine if there exists a need for interim improvements while the long-term program continues has led the staff to recommend a two-step process. The first step is to establish a screening criterion based on RT_{NDT} to identify reactor vessels where radiation embrittlement has progressed to the point of potential concern. This criterion was selected using simplifications and generic treatment of certain design features, transients, fracture mechanics analysis and plant operating characteristics as described in Sections 2, 3, and 4. The second step, to be taken for plants with vessels with values of RT_{NDT} that exceed or are approaching the screening criterion, involves more detailed plant-specific analyses to determine what, if any, modifications are necessary to the plant design and/or operations to resolve the concern.

The purpose of this section is to outline the analyses and actions to be required of those licensees whose reactor vessels have exceeded the RT_{NDT} screening criterion or will exceed the screening criterion within three calendar years. More detailed requirements must be formulated by the staff in the near future so that it will be clearly understood what methods of analysis are acceptable to the staff and what level of detail is required. Further, and most important, acceptance criteria will be developed and promulgated regarding the required analyses and actions.

9.2 Evaluation of Overcooling Event Sequences

Assessment of pressurized thermal shock concerns on a plant-specific level requires a study of the unique potential for and consequences of severe overcooling transients at the specific plant. The overcooling transients must be chosen for analysis based on a detailed plant-specific control and safety system design, procedural, and human factor study. The study must include a systematic search for, and identification of, potential overcooling event sequences to identify those sequences which are the dominant contributors to the risk of pressure vessel failure. The generic studies of potential event sequences done thus far by the staff and by the Westinghouse Owners Group, described in Section 6 of this report, have shown that consideration of only the design basis accident sequences conventionally presented in Safety Analysis Reports does not identify adequately these dominant sequences. The design study must include systems functions pertinent to cooldown transient sequences and must include such systems as the feedwater system, steam generator level control system, steam dump system, steam generator power operated relief valves, charging and letdown system, emergency core cooling system, monitoring instrumentation, and control and safety systems actuation instrumentation. The procedural and human factors study must include operating and emergency procedures, instrumentation available to the operators, operator training, and the ability of the operators to diagnose transients and accidents that could or do result in a rapid cooldown of the primary system.

The purpose of this study is twofold. First, the results of this study will be used in event-tree analyses which would identify failures that could initiate cooldown transients and quantify the frequency of these events and end states. This information will then be used to select those events that should be subjected to detailed thermal-hydraulic analyses to determine the cooldown rates and end states in characteristic pressures and temperatures, which will be used in fracture mechanics analyses whose results will help determine risk.

Second, the results of this study should identify systems, instrumentation, material, and procedural and training program improvements necessary to reduce the probability and consequences of pressurized thermal shock events.

9.3 Vessel Materials Properties (Refer to Appendix E for background and detail)

Available information on the vessel properties should be re-examined in detail to fill any gaps in the supporting data for making an estimate of RT_{NDT} and to support resolution of any disagreements about the validity of values used.

9.3.1 Improve Basis For Initial RT_{NDT}

As noted in Section 5.2, and discussed in more detail in Appendix E, for many older reactor vessels, few data are currently readily available and validated to support the selection of a value for the initial RT_{NDT}. The confidence that can be placed in estimates of the initial RT_{NDT} depends not only on metallurgical tests, but also on the accurate documentation of welding technique, weld wire used, and weld flux used. The credibility of such estimates could be enhanced by performing more tests on archival material, by discovering previously unreported test results on weld specimens from the particular plant, or by evaluating properties of welds considered typical of the plant-specific weld.

9.3.2 Refinement of Chemistry Information for Critical Materials

If it was necessary to assume 0.35 percent copper, because there was no other information, attempts should be made to find archival material suitable for chemical analysis, or data on the weld material from other vessels where it may have been used. If the surveillance material matches one of the critical welds, some check analyses for copper and nickel contents of broken Charpy bars should be considered.

9.3.3 Vessel Fluence (See Appendix F)

Fluence calculations for the critical welds should be rechecked, using modern codes and information from surveillance dosimetry. Location of critical welds relative to the axial and azimuthal flux map should be taken into account, as well as changes in fuel loading during periods when dosimeters were exposed.

9.4 Deterministic Fracture Mechanics Evaluations (See Appendix D)

For the limiting transients as determined in 9.2 and materials properties as determined in 9.3, licensees should provide sufficiently detailed fracture mechanics analyses to permit the NRC staff to interpret the results without its having to redo the calculations. The details should include a listing of the assumptions used, the bases for them and a discussion of the sensitivity of the results to variations in the assumptions. Items to be discussed are:

- Vessel wall thickness and clad thickness; vessel inner radius
- Location and orientation of the assumed initial crack
- Heat transfer coefficient used and material properties, k, $(\frac{E\alpha}{1-\gamma})$ vs. temperature
- Assumed crack shape at initiation and time(s) of initiation
- Crack shape at arrest
- Treatment of cladding-induced stresses
- Upper shelf toughness
- Bases for the determination of limiting RT_{NDT} (at the inner vessel radius)

The results of each transient analyzed should be portrayed as a plot of critical and arrest relative crack depths versus time into the transient. Superpose a line indicating when warm prestressing is deemed to be effective and a curve indicating the depth at which the upper shell toughness is reached. If crack arrest is predicted and accepted at or above the upper shelf, it must be justified.

9.5 Flux Reduction Programs (See Appendix I)

A technique involving core fuel loading patterns should be investigated as a method for reducing neutron flux at the reactor vessel wall and at critical weld locations. This would reduce the rate at which the reactor vessel experiences a decrease in ductility and fracture toughness properties. Particular areas of concern in the reactor vessel should be located from an analysis of the material properties of the reactor vessel plate and weld metals. Consideration should be given to replacing fuel assemblies in close proximity to these critical areas. To reduce flux levels these fuel assemblies could be replaced by spent fuel, zircaloy or stainless steel spacers, or water. Another scheme to be investigated would be an in-out loading pattern where fresh fuel is loaded into the center of the core and moved outward in later cycles. Implementation of revised fuel management techniques have demonstrated a reduction in the neutron flux at the positions of previous maxima by factors of approximately two without derating the reactor power level.

9.6 Inservice Inspection and Nondestructive Evaluation Program (See Appendix L)

Current requirements specified in 10 CFR 50.55a endorse ASME Section XI as defining the examination requirements for reactor vessel welds. The volume of weld to be examined includes the near-surface area; however, the inspection equipment calibration requirements are not providing sensitivity sufficient to detect near-surface cracks. As a result, currently employed techniques do not provide sufficient basis for assuming that all near-surface cracks can be detected.

The utilization of state-of-the-art nondestructive evaluation techniques provides an opportunity to decrease or eliminate a conservatism used in the generic assessment of pressurized thermal shock; that is, small cracks exist at or near the surface of the reactor vessel. A feasibility study should be performed for using state-of-the-art examination techniques for inspecting the clad-base metal interface and the near-surface area. This would include plant-unique consideration of the clad surface conditions and may require grinding the clad metal smooth enough to utilize these techniques.

9.7 Plant Modifications

To adequately protect reactor vessels from the effects of pressurized thermal shock, the protection needs to be compatible with the plant design and commensurate with the vessel's fracture toughness properties and/or susceptibility to cooldown transients. Modifications to be considered should include the following:

(1) Instrumentation and Controls (See Appendix J)

(a) reactor vessel downcomer water temperature monitor

(b) instantaneous and integrated reactor coolant system cooldown rate monitors
(c) steam dump interlock
(d) feedwater isolation/flow control logic
(e) reactor coolant system pressure and temperature monitors
(f) NDT margin monitor

(2) Automatic Depressurization Logic

(3) Increased Emergency Core Cooling Water and Emergency Feedwater Temperatures. (See Appendix K)

Because of design differences and transient response characteristics, plant-specific consideration should be given to any system modifications. Further, for active system modifications such as an automatic depressurization system, a failure mode and effects analysis should be performed to verify that inadvertent operation of the system would not induce transients more severe than the mitigative capabilities of the plant's safety systems or that otherwise create an unacceptable risk.

9.8 Operating Procedures and Training Program Improvements (See Appendix C)

As a result of generic pressurized thermal shock event tree analysis and actual reactor operating experience it has been shown that operator actions and associated plant response play a key role in the initiation and mitigation of pressurized thermal shock events. The seven plants currently being evaluated by the NRC for susceptibility to pressurized thermal shock have reviewed these current operating procedures for information relevant to the pressurized thermal shock issue. Based on the NRC's and the licensee's review of their own procedures, a number of revisions have been incorporated.

The following list includes those types of procedural modifications that should be considered.

(1) Procedures should not instruct operators to take actions that would violate NDT limits.

(2) Procedures should provide guidance on recovering transient or accident conditions without violating NDT or saturation limits.

(3) Procedures should provide guidance for recovering from PTS conditions.

(4) Pressurized thermal shock procedural guidance should have supporting technical bases.

(5) High pressure injection and charging system operating instructions should reflect consideration of pressurized thermal shock.

(6) Feedwater and/or auxiliary feedwater operating instructions should reflect pressurized thermal shock concerns.

(7) Training should include specific instruction on NDT vessel limits for normal modes of operation, transients and accident conditions.

(8) Training should particularly emphasize transients and accidents known to require operator actions to mitigate pressurized thermal shock.

(9) Training should include simulator operation responding to potential pressurized thermal shock transients and accidents.

9.9 <u>In-Situ Annealing</u> (See Appendix M)

Annealing of the reactor vessel is a possible, although difficult and expensive, remedial measure for the radiation embrittlement problem. Research sponsored by both the regulated industry and the NRC has provided a basis for selecting the temperatures and duration of the annealing process with some data on reirradiation damage. Research is being funded by the Electric Power Research Institute on the feasibility of annealing. A draft report on annealing proposes the use of electric resistance heating elements supported by a frame that can be lowered into the reactor vessel. The draft report on this study finds no insurmountable difficulties; however, many engineering details remain to be resolved. These include the potential for vessel damage, and protecting the concrete and vessel support structures from the effects of high temperatures. For those plants where proposed remedial actions of the types described in Sections 9.2 through 9.8 above do not result in acceptable risks of vessel failure for the whole design lifetime, a plant-specific engineering evaluation of in-situ annealing should be performed.

9.10 Basis for Continued Operation

Finally, as part of the plant-specific analysis package, the licensee will provide a basis for concluding whether or not continued plant operation is justified while any corrective actions needed to meet the acceptance criteria are planned and implemented.

This basis should include details regarding frequency of PTS events, description of the dominant risk contributors, and assessment of the total risk from all such events. Vessel and containment failure modes should be discussed, and it should be shown quantitatively how such considerations are factored into the overall risk assessment. The total projected PTS risk for the interim period until acceptance criteria can be met by corrective action should then be compared to the NRC safety goal.

10.0 CONCLUSIONS AND RECOMMENDATIONS

10.1 Conclusions

As a result of evaluations performed thus far of the issue of pressurized thermal shock, the NRC staff has reached the following conclusions:

(1) There is no need for immediate modification of any operating pressurized water reactor.

(2) Further, more detailed, plant-specific evaluations will be needed in the near future for selected plants to determine what, if any, modifications to equipment, systems and procedures should be required, and on what schedule, to provide sufficient protection against vessel failure from PTS events for the remainder of the plant design life.

(3) A screening criterion is needed to select the plants for which plant-specific evaluations should be required, and to establish the schedule for submittal of the evaluations. Based on the technical evaluations presented in this report, the staff recommends screening criteria values of RT_{NDT} of 270°F for axial welds and 300°F for circumferential welds. The present and projected values of RT_{NDT} to be used for a given vessel should be determined by the method described in Section 5 and Appendix E of this report.

(4) Whenever the value of RT_{NDT} for a given vessel is projected to exceed the screening criteria within the next three calendar years, the licensee of that plant should be required to submit plant-specific evaluations of the type described in Section 9 of this report. In the near future, the staff should develop more detailed guidance for these evaluations and acceptance criteria for determining whether plant modifications are needed based on the evaluations.

(5) Some of the Commission's regulations (Appendix G to 10 CFR Part 50, 10 CFR 50.46, and possibly others) may not appropriately reflect current understanding of the state of reactor vessel embrittlement and the potential for vessel failure as a result of PTS (see discussion in Section 8). Timely consideration should be given to the possible need for amendments to the regulations.

10.2 Recommended Near-Term Actions

The NRC staff recommends that the Commission approve the following near-term actions:

(1) An RT_{NDT} Screening Criterion should be promulgated by generic letters to all PWR licensees, or by a Commission Policy Statement.

(2) Licensees of all operating PWRs should be required to submit a determination of the present RT_{NDT} values for their reactor vessels and the estimated date at which the RT_{NDT} value will exceed the screening criterion. This requirement could be issued by generic letters, orders, or by regulation.

(3) Licensees of operating PWRs for which the RT_{NDT} value is projected to exceed the screening criterion within three calendar years of the date of promulgation of the criterion or regulation should be required to submit plant-specific evaluations within a specified time.

This requirement could be issued as a request for information pursuant to 10 CFR 50.54(f) to enable the Commission to determine whether the license should be modified, suspended or revoked; or as part of an order or regulation if that option is taken under (2) above.

(4) Within the next several months the staff should develop and issue more detailed guidance to licensees on the information to be provided in the submittals required by (3) above.

(5) The staff and the Commission should give timely consideration to the possible need to amend certain of the regulations to better reflect the potential for PTS.

10.3 Recommended Longer-Term Actions

(1) The ongoing program to improve procedures and operator training regarding prevention and mitigation of PTS events should continue, as described in Appendix C of this report.

(2) Industry and NRC programs are needed to provide additional confirmatory PTS information, to decrease the large uncertainty of current PTS analyses, to extend the analysis to B&W and CE plants, and to investigate more thoroughly the alternatives to delay and mitigate PTS risks. In particular, the analytical and experimental studies underway as part of the NRC research program, as described in Appendix N of this report, should continue on a high-priority basis. These programs should improve the staff's capability for independent audits and assessments of licensee evaluations, confirm or improve calculational methods and assumptions, and aid in further assessments of safety margins.

(3) The best available methods should be used for periodic in-service inspection of high-RT_{NDT} vessels, to maximize the likelihood of detecting any flaws that may be present relevant to PTS.

(4) A more vigorous industry effort is needed to minimize neutron leakage flux and thus to minimize the increase in RT_{NDT} for all vessels with high copper content. Risk-cost-effective changes should be sought with greater flux reduction than those from the "low-leakage cores" now being used primarily to minimize overall operating costs.

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APPENDICES TO SEPTEMBER 13, 1982 DRAFT

NRC STAFF EVALUATION

OF

PRESSURIZED THERMAL SHOCK

(SEPTEMBER 15, 1982)

LIST OF APPENDICES

*To be provided

APPENDIX A

OVERVIEW OF ACTIVITIES CONCERNING THE PRESSURIZED THERMAL SHOCK ISSUE

A.1 INTRODUCTION

The subject of thermal shock to reactor pressure vessels from overcooling transients is not a new concern; both industry and the NRC have held meetings and issued written reports on the subject for several years. The thermal shock concern after a Loss of Coolant Accident (LOCA) has been subject to considerable review in the past. Analyses and experiments indicate that the vessel will still hold water after a large LOCA. Therefore, for large LOCA, thermal shock to the reactor pressure vessel is not a new concern.

The TMI Action Plan (NUREG-0737, Item II.K.2.13 "Thermal Mechanical Report Effect of High-Pressure Injection on Vessel Integrity for Small-Break Loss of Coolant Accident with No Auxiliary Feedwater") identified one transient of concern which is characterized by severe overcooling causing thermal shock to the vessel, concurrent with or followed by repressurization (that is, Pressurized Thermal Shock, PTS). The staff has recognized that there are many other scenarios which could result in PTS. On the basis of events which have occurred at operating PWRs, the staff recognized early in 1981 that some operating reactor pressure vessels of the older plants were approaching material property conditions which made the PTS issue a greater concern. Thus the NRC staff requested a meeting with industry representatives on March 31, 1981, to discuss the PTS problem. This initiated the current effort concerning the PTS issue.

The PTS issue is a concern only for operating PWRs. Boiling water reactors (BWRs) do not have a significant PTS concern. BWRs operate with a large portion of water inventory inside the pressure vessel at saturated conditions. Any sudden cooling will condense steam and result in a pressure decrease, so simultaneous creation of high pressure and low temperature is improbable. Also contributing to the lack of PTS concerns for BWRs is the lower fluence at the vessel inner wall and the use of thinner vessel wall which results in a lower stress intensity for a postulated crack.

The attached appendix provides a time table of events concerning the PTS issue.

A.2 Summary of Industry Meetings with the Staff

On March 31, 1981,[1] the NRC staff met with the PWR Owners Groups and representatives of NSSS vendors to discuss the effects of potential thermal shock to reactor pressure vessels by overcooling transients and the potential consequences of subsequent repressurization at relatively low temperatures. The staff requested the industry to make a plant-by-plant assessment of the problem and to scope and bound the problem. As a result of this meeting the industry representatives committed to a report by May 15, 1981, providing an account of what immediate problems exist. Subsequently, by letter dated April 20, 1981,[2] the NRC requested the Owners of PWR operating plants to respond by May 22, 1981, identifying the specific action which the plant Owners propose to take.

Meetings were held with Babcock & Wilcox (B&W), Westinghouse (W) and Combustion Engineering (CE) Owners Groups (OG) on July 28, 29, and 30, 1981,[3] respectively, at the request of the staff in order to present the staff's analysis of the problem and the actions the staff intends to take and to hear from the PWR Owners the results of their analyses and their proposed actions concerning the problem. The staff concluded at this meeting that Owners of plants of each NSSS type which have the highest RT_{NDT} values would be requested to take action to resolve the problem for their plants. Subsequently, the NRC requested under 10 CFR 50.54f, by letters dated August 21, 1981,[17] the licensees of Oconee 1, TMI-1, Robinson 2, Turkey Point 4, San Onofre 1, Calvert Cliffs 1, Fort Calhoun and Maine Yankee (1) a 60-day response for information related to RT_{NDT} and operator action to prevent PTS and ensure vessel integrity and (2) a 150-day response for information which would define actions and schedules for resolution of the PTS issue and analyses to support continued operation.

Follow-up meetings were held with the W, B&W, and CE OGs on September 18 and 22, and October 7, 1981,[4,5,6] respectively, to review the progress and to discuss the technical issues concerning the systems analyses, operator responses, and the materials and fracture mechanics aspects of the PTS issue.

The WOG indicated their report for the TMI Action Plan Item II.K.2.13 due at the end of the year would address (1) the Small Break Loss of Coolant Accident with Loss of Feedwater (SBLOCA + LOFW) (TMI II.K.2.13) and other scenarios including steam line breaks, (2) fracture mechanics calculations for each operating plant, (3) the date and RT_{NDT} for each plant when acceptable conditions will not be met, and (4) evaluation of remedial actions. WOG indicated that the most limiting plant has at least 3 EFPY remaining before there is a concern.

The B&WOG indicated that their work would be concentrated on the Oconee 1 150-day response to the August 21, 1981 letter and plant-specific analyses thereafter.

The CEOG indicated that the report due at the end of the year would address the TMI Action Plan Item II.K.2.13 and other scenarios including the main steam line break event. The CEOG indicated that the most limiting plant has at least 5 EFPY remaining before there is a concern assuming the no-crack initiation criteria.

Meetings were held with the WOG and CEOG including the Owners of the six selected plants who received the August 21, 1981 letter on February 24 and March 3, 1982,[7,8] at the request of the staff. A meeting was held with Duke Power Company on March 24, 1982.[9] These meetings were to discuss the respective Owners groups' reports and the "150 day" responses concerning San Onofre 1, Robinson 2, Turkey Point 4, Fort Calhoun, Calvert Cliffs 1, Maine Yankee, and Oconee 1. TMI-1 was not included in these discussions since GPU elected to delay their "150 day" submittal until June 1982. These meetings were designed to respond to specific staff concerns which were identified with the published meeting notices and later were, in part, transmitted to the Owners of the selected plants.[34-41]

Symbol 34-41

Meetings were held with the Omaha Public Power District and the WOG on May 6, and 10, 1982,[10,11] respectively at their request to update the staff on the progress of the respective programs and the responses to the staff's concerns identified in the previous meetings.

The WOG provided the results of a study involving a methodology leading to a probabilistic risk assessment (PRA) related to PTS. The conclusion of this study was that the likelihood of a cooldown transient can challenge the reactor vessel is less than 10^{-4} to 10^{-3} per reactor year for that lead plant at 5 EFPY from today. WOG maintains that the total risk to public health is in the area of 10^{-9}.

The CEOG provided responses to the staff concerns identified in the meeting of March 3, 1982. In particular the CEOG provided the results of their review of operating experience of CE operating plants and the results of a probability analysis related to the PTS issue. The review representing 49 reactor years of operating experience identified 16 events which met a screening criterion. Of those only two met the selection criteria. These actual overcooling events were much less severe than the event analyzed in the "150 day" response and there was no uncontrolled repressurization in either event. The probability study concluded that the main steam line break (MSLB) is the most severe event and ranges between a probability of 10^{-6} to 10^{-4}.

A meeting was held on June 2, 1982,[12] with General Public Utilities (GPU) at their request to provide the staff a status report on the PTS program for TMI-1 and to present a summary of the "150 day" response for TMI-1. Significant in this study was the use of the COMMIX Code in the mixing analysis. The COMMIX Code shows warmer temperatures for the SBLOCA events than the BAW 1648 or Oconee 1 mixing models. The SBLOCA and turbine bypass valve failure were the only events analyzed. GPU determined that based on EOL RT_{NDT} of 335°F for the most critical weld, operation would be acceptable for 32 EFPY.

A meeting was held on June 9, 1982,[13] with the PWR industry representatives at the request of the staff for the purpose of discussing the current NRC staff considerations of possible recommendations for PTS requirements. The staff was considering a limit of T_f RT_{NDT} of 230°F for longitudinal welds and 255°F for circumferential welds based on a transient which resulted in a final temperature/pressure of 250°F/2500 psi ($\beta = 0.15$) which would initiate a crack. The industry representatives did not agree with the conservatism of the staff considerations. They objected to the crack initiation criteria. They believed the final temperature was too low and the pressure was not possible. They objected to the data base which was used for the probabilistic determinations. The staff provided the industry two weeks to submit comments in order for staff to consider the industry news in the determination of the staff's position.

Meetings were held on June 22 and 23, 1982,[14,15] with the WOG and CEOG respectively at their request to respond to the staff's request for comments to the staff's proposed recommendations on PTS requirements. WOG proposed a screening criteria of a RT_{NDT} of 310°F and 335°F at longitudinal and circumferential welds respectively. This criteria was based on T_f = 290° at the surface of the reactor vessel weld. The WOG PRA and the NRC probabilistic fracture mechanics was coupled with the W probabilistic transient evaluation to yield safety goals somewhat lower than those reported by the staff. The WOG analysis indicated that the PTS issue would be of no concern to operating plants for the transient for the next five years of plant operation.

The CEOG recommended the use of CEN-189 best-estimate initial RT_{NDT} values. They recommended the current Regulatory Guide 1.99 but used to predict the upper bound shift for high-copper, high-nickel material at fluence greater than 10^{19} nvt and that Guthrie (HEDL) correlation be used to predict the upper bound shift for medium-low copper, high-low nickel material at fluence less than 10^{19} nvt. The CEOG believes arrest will occur. The probabilistic analysis indicated that the MSLB bounds the PTS events.

A meeting was held with the WOG on July 30, 1982,[100] at the request of the staff to discuss the apparent discrepancies between the WOG and the staff concerning the limiting transients which produce the greatest overcooling, the frequencies of such transients, and the fracture mechanics analysis associated with the transients. In particular, the meeting discussed the small break LOCAs (SBLOCAs) which result in stagnation flow and the factors in the fracture mechanics analysis which account for the differences between the WOG and the staff. WOG indicates that SBLOCAs in the area of 2" to 3" were the sizes of concern which result in stagnation flow and the frequencies of such events were conservatively estimated to be 6×10^{-4}. Factors which account for the differences in the fracture mechanics analyses were heat transfer coefficient used, crack length assumptions, and effects of the clad. WOG assumed the heat transfer coefficient was not 300° continuous through the weld. It varies as explained in WCAP 10019. WOG assumed an elliptic crack versus the staff's assumptions of an infinite long crack. WOG assumed the clad has no effect.

A follow-up meeting[101] was held with the WOG on August 11, 1982. WOG indicated that a lower limit for the SBLOCA of concern was 5×10^{-5} (a medium value). More realistic mixing assumptions concerning other factors such as metal heat resulted in approximately 60° increase to prior results of analysis of the SBLOCA.

For the longitudinal flaw the calculational differences between the staff and the WOG amount to 45° RT_{NDT}.

The staff proposed a screening criteria as follows:

T_F = 260°F at 10^{-2} frequency
RT_{NDT} = 270° for longitudinal welds
RT_{NDT} = 300°F for circumferential welds.

The above is based on operating references.

A.3 Summary of Industry Responses to Staff Requests

At the meeting of March 31, 1981, with the PWR industry representatives, the PWR Owners Groups agreed to provide individual owners groups reports by May 15, 1981, which would provide an accounting of what immediate problems exist. By letters dated April 20, 1981,[2] the NRC requested the Owners of operating PWR plants to provide responses by May 22, 1981 relating to their participation in the Owners groups programs and specific action which they intend to take. By letters dated August 21, 1981,[17,6] the NRC requested 30-, 60- and 150-day responses from each of eight selected utilities owning plants which represented the three different vendor NSSS and reactor vessels with the highest irradiation damage of each group.

The NRC responded[18-41] to each of the utilities responses to the August 21, 1981 letter. As a result of the 60- and 150-day responses the staff requested additional information from each utility which received the August 21, 1981 letter.

Each of the Owners groups provided responses by May 25, 1981[42-43] with an accounting of the immediate concern and their plans for resolving the issue. Owners of all operating PWR plants indicated their participation in the Owners groups programs by letters in response to the NRC letter dated April 20, 1981.

Tables A-1, A-2 and A-3 provide the summaries of the 30-, 60- and 150-day responses, respectively[45-66] of the selected utilities which received the August 21, 1981 letter. Table A-4 provides a summary of the W and CE generic reports[67-68] concerning the PTS issue.

A.3.1 Responses Relating to Westinghouse Plants

The WOG response dated May 14, 1981 from Mr. Robert W. Jurgensen[44] indicated that all Westinghouse operating plants could sustain severe thermal shock transient, including repressurization to beyond January 1983. The WOG program would be completed by December, 1981. Each utility of a Westinghouse plant would provide additional information including a schedule for remedial action if requested on completion of the WOG program.

Tables A-2, A-3 and A-4 provide summaries of the "60 and 150 day" responses and the generic reports. These responses were supplemented by additional information received from the WOG and each of the three selected Westinghouse operating plants in May 1982.[69-72]

The Westinghouse WOG report concludes that a number of reactor vessels will require more plant-specific evaluations and may require that remedial actions be implemented at some point in the vessel life to demonstrate vessel integrity to end-of-life. The licensees of the three Westinghouse reactors all concluded that vessel integrity will be maintained to or beyond end-of-life.

The supplemental information provided by the WOG at the meeting of May 10, 1982,[11] concludes that the probability of a transient of PTS concern for the "lead" plant at 5 EFPY from today is between 10^{-4} to 10^{-3}.

By letter dated May 28, 1982,[69] the WOG provided supplemental information on Reactor Vessel Integrity in the form of a report "Summary of Evaluation Related to Reactor Vessel Integrity." This report supported the conclusions provided by the WOG at the meeting of May 10, 1982.

By letter dated June 16 1982,[73] the WOG provided a discussion of benefits and penalties of fuel management schemes to reduce fluence in the form of a report "Fuel Management To Reduce Neutron Flux." This report provides methods of reducing the flux to the pressure vessel with no power derating or economic penalty.

By letter dated June 22, 1982,[14] the WOG provided the "Review of the Emergency Response Guidelines Related to Pressurized Thermal Shock." This report explicitly identified those steps in the Emergency Response Guidelines (ERG) that have been written to provide operator direction to prevent a mitigated PTS event. The report also determined those areas of the ERGs that should be modified to more clearly identify appropriate operator responses to prevent or mitigate potential PTS events.

A.3.2 Responses Related to Combustion Engineering Plants

The CEOG response dated May 15, 1982, from Mr. K. P. Baskin[43] indicated that the steam line break transient produces the largest magnitude and rate of heat removal for the CE-NSSS design. With this transient, approximately 5 EFPY of operation would have to elapse before vessel integrity would theoretically become a concern.

The CEOG response indicates that a program is planned to address all aspects of the PTS issue and a generic response to TMI Action Plan Item II.K.2.13 would be provided by January 1, 1982.

The Combustion Engineering CEOG Generic Report[68] concludes that all CE plants can withstand the postulated small break LOCA (SBLOCA) with extended Loss of Feedwater (LOFW) scenarios for the assumed life of the plant. The 150-day responses[60-62] from the three licensees of operating CE plants all indicate that vessel integrity will be maintained for the lifetime of the plant.

The supplemental information provided by each of the three selected CE operating plant owners[75-77] indicated that the main steam line break event is the most limiting event and ranges between a probability of 10^{-6} to 10^{-4}. They also provided an identification of overcooling events from the operating history of CE plants. In addition the responses discussed the sensitivity of controlling overcooling transients to operator action.

By letter dated June 14, 1982, the CEOG provided a response to the NRC staff proposed position that was presented at the June 9, 1982 meeting. This letter reiterated the CEOG conclusion that the MSLB event is the most limiting and probable concerning the PTS issue. The CEOG concludes that an RT_{NDT} value of 320°F is more appropriate for crack initiation criteria for the NRC proposed transient (T_F = 250°, P = 2500 psi, β =0.50m^{-1}).

The NRC staff proposed crack initiation criteria was considered unnecessarily conservative. The ability of the CE-NSSS to cool down as rapidly as the NRC proposed temperature transient while maintaining pressure at 2500 psi is considered physically impossible. The CEOG contends that the NRC calculated probability of the NRC proposed temperature transient is much too high. The CEOG disagrees with the approach taken by the NRC to resolve the PTS issue.

Omaha Public Power District provided comments concerning the staff's proposed position by letter[79] dated June 26, 1982. OPPD suggested that some type of screening criteria would be appropriate to focus on plants which might develop a potential PTS concern. The screening criteria should reflect the assessment of plant operating histories and major design differences. The use of best-estimate RT_{NDT} value is most appropriate.

A.3.3 Responses Related to B&W Plants

The B&WOG response dated May 12, 1981 from Mr. John J. Mattimoe[42] indicated that the SBLOCA with no repressurization is the bounding accident. This assumes operator action would mitigate repressurization (by throttling HPI and utilizing atmospheric dump or turbine bypass valves). B&W contended that the analysis is conservative and there is no concern for thermal shock through 1982. The B&W Owners submitted, in December 1980, BAW 1648, which addressed TMI Action Plan Item II.K.2.13 "Thermal Mechanical Report - Effect of HPI on Vessel Integrity for SBLOCA with Additional Loss of Feedwater." The B&WOG plans with respect to PTS to submit plant-specific analyses to address the conservatisms in the generic analysis. No generic report was planned for B&W plants.

Oconee 1 and TMI-1 were the B&W selected plants for the August 21, 1981 letter.

The 150-day response from Duke Power Company[66] concerning the Oconee 1 vessel concludes that no changes to the plant or additional fuel management, or reactor vessel annealing is necessary to assure safe operation of Oconee 1 through the design life of the plant. The Oconee 1 report indicated that severe PTS events were in the probability range of 10^{-6} to 10^{-4}. The Duke Power Company letter dated April 30, 1982[80] provided additional information concerning operator responses and sensitivity of transient analysis to operator action times.

By letter dated March 17, 1982,[81] GPU Nuclear informed the staff that the "150 day" response concerning TMI-1 would be submitted as soon as the revised mixing analysis could be inputted into the B&WOG plant-specific analyses (estimated completion June 1982).

By letter dated June 1, 1982,[82] GPU provided a response to the NRC letters of August 21, 1981, and December 18, 1981. This letter provides a summary of an analysis which GPU proposed to provide at the end of June, 1982. The summary concludes that the TMI-1 reactor pressure vessel integrity will not be compromised due to PTS events during the lifetime of the plant. Also the rate of embrittlement of the TMI-1 vessel may be reduced further if the plant switches to low leakage fuel scheme in the near term reloads. GPU indicated because of the concerns by the PTS issue, operator response will be significantly improved through increased awareness and additional training.

By letter dated June 22, 1982,[83] the B&WOG provided a response to the NRC staff's request at the meeting of June 9, 1982 concerning the NRC staff proposed position on the PTS issue. The B&WOG indicated that the generic position is unsound, unrealistic, and inappropriate unless used solely as a screening basis. Also the NRC proposed crack initiation criteria was considered to be highly conservative and the proposed generic transient does not realistically represent an actual B&W plant response. The B&WOG recommended the use of RT_{NDT} as a means of "flagging" plants with potential concerns. Each plant should be analyzed for a realistic probable transient.

Letters[84-88] were received from Duke Power Company, Arkansas Power & Light Company, Florida Power Corporation, GPU Nuclear and SMUD in response to the staff's request concerning the proposed staff position concerning the PTS issue. Duke Power Company indicated that the staff proposed approach can be utilized as a screening method of identifying plants for detailed analyses with respect to the PTS issue. However, the staff's analysis of the frequency of the transient events is not applicable to any real plant. The Duke Power Company letter expressed the concern that the staff has failed to provide a feedback loop such that plant improvements made are directly included in the analyses. Also the screening criterion may need to be established on a group of plants or even on an individual plant basis rather than a generic PWR basis.

Arkansas Power & Light Company's comments[85] on the staff proposed position follows:

(1) Indexing the operation to actual fracture toughness rather than on RT_{NDT} should be pursued.

(2) RT_{NDT} could be used as a screening criteria.

(3) The staff's generic approach does not consider basic design differences.
(4) The transient selected by the staff is unrealistic.
(5) The crack initiation criteria is not consistent with the ASME Code.
(6) AP&L disagrees with some of the basic assumptions of the staff's proposal.

Florida Power Corporation[86] included the following comments on the staff's proposal:

(1) System pressure does not remain constant as proposed by the staff.
(2) Emphasis should be focused on actual fracture toughness rather than RT_{NDT}.

GPU[87] offered the following comments to the staff's proposal:

(1) The staff's proposed failure criteria is too conservative.
(2) The proposed governing transient is too severe and overly conservative.
(3) Emphasis should be placed on the actual toughness of the vessel material rather than RT_{NDT}.

SMUD indicated that the PTS issue cannot be realistically evaluated by focusing on a single parameter such as RT_{NDT}.

A.4 NRC Staff Audit of Operating Procedures, Operator Qualifications and Training With Respect to the PTS Issue

On March 16, 1982, a NRC short-term task force on PTS was organized to make a detailed review and prepare a report on the efforts on PTS at the H. B. Robinson Nuclear Plant. Specifically, the task force was to provide a report characterizing the problems, methodology of resolution, bases for conclusions and recommendations regarding the adequacy of in-place training programs and operating procedures.

The report[89] "NRC Staff Audit of Robinson and Procedures and Training for Pressurized Thermal Shock" dated April 15, 1982, recommended that prior to restart the Robinson 2 operators and STAs should be retrained in areas related to the PTS issue and that SI termination criteria and procedures be changed to accommodate the PTS issue.

The task force also recommended similar audits be performed at the other seven plants which were identified with the August 21, 1981, letter.

By letter dated April 26, 1982,[90-95] the seven other utilities of plants of concern were requested to cooperate in this effort.

A.4.1 Robinson 2 Audit

A visit to the Robinson 2 site took place on April 5-7, 1982, to evaluate procedures and training. By letter dated April 20, 1982,[96] the staff confirmed the understanding that of general acceptance of the recommendations of the task force report. This was confirmed in writing by CP&L by letter dated May 4, 1982.[97]

A.4.2 Oconee 1 Audit

A review of Oconee's procedures and training for PTS was conducted May 11-13, 1982. In general, the review team found the operators adequately knowledgeable of the PTS issue, except that knowledge of past PTS events at other facilities was weak. The procedures provided mitigative actions to prevent PTS, but needed to be strengthened to provide actions if an unacceptable pressure/ temperature condition was reached. The audit team felt that a means should be provided for plotting cooldown rate and subcooling margin with the plant computer out of operation.

A.4.3 Fort Calhoun Audit

A review of the procedures and training for PTS at Omaha Public Power developments Fort Calhoun plant was completed on June 8-10, 1982 by PNL. General recommendations[99] regarding procedures and control instrumentation made by PNL included:

(1) The values of parameters of interest in procedures should be consistent as appropriate.
(2) Emergency procedures should note both minimum and maximum subcooling temperatures.
(3) Emergency procedures should identify only one form of saturation curve.
(4) The current NDT curve should be in every procedure which references it.
(5) The subcooling margin indications should be available for all ranges of RCS temperatures.

A.4.4 San Onofre 1 Audit

An onsite audit was conducted of the San Onofre Unit 1 procedures and training for PTS June 2-4, 1982. Preliminary findings from the audit indicate that the procedures are based on plant-specific analyses of transients and that the operations personnel were familiar with PTS even though their training was not completed at the time of the audit. The findings indicate that the remainder of the training program should include instruction on past cooldown events. Findings on the San Onofre 1 procedures are included in the audit report. The procedures were generally found adequate for PTS considerations, and were based on Westinghouse analysis. The findings indicate that a method for plotting cooldown rate should be provided to the operators.

4.4.5 Maine Yankee Audit

A review of Maine Yankee's procedures and training for PTS was conducted on May 25-27, 1982. The review team found the plant operations personnel and STAs adequately knowledgeable of the PTS issue, and the procedures provided adequate guidance for preventing PTS. One significant operating philosophy already in place at Maine Yankee is the throttling of HPI flow to maintain as close to 50°F subcooling as possible during potential cooldown events. It was noted by the review team that no written exam was conducted after the lectures on PTS. Rather, a seminar method was used to determine the level of comprehension. Questions regarding PTS have been included in the written requalification examinations. The review team concluded that the operators were sufficiently knowledgeable of PTS. No changes to the operating procedures or training program were recommended to meet the objectives of the audit.

Table A-1 Summary of responses to NRC letters dated August 21, 1981 concerning thurmal shock issue
October 7, 1981

Plant Name (Date of licensee's 30 day response to ltr of 8/21/01)	Licensee's 30 day response concerning 5 items to be submitted in 60 days					Licensee's 30 day response concerning the 150 day response.	Remarks
	(1) Request for present RT_{NDT} plates and welds	(2) Rate of RT_{NDT} increase	(3) RT_{NDT} limit for continued operation	(4) Basis for RT_{NDT} limit	(5) Questions concerning operator actions		
Calvert Cliffs 1 (September 24, 1981)	Will be answered within 60 days	Will be answered within 60 days	Do not think a a simple RT_{NDT} value is an appropriate limit.	Will provide a more reasonable criteria for a limit for continued operation in 150 day response.	Will be answered in 60 days	Will provide a plan within 150 days. Item 7 of request for additional information will not be complete. Will provide a schedule for complete response.	NRC letter dated 10/22/81 indicated staff would accept what licensee will provide. Staff will continue effort which may result in specifying conservatisms where more definitive info. is not available.
Fort Calhoun (September 22, 1981)	Will provide value for plate material within 60 days. Will provide value for weld material in 150 day response.	Will provide value for plate material within 60 days. Will provide value for weld material in 150 day response.	Do not think a simple RT_{NDT} value is an appropriate limit.	Will provide a response within 60 days. Response will provide basis for response to (3).	Will be answered in 60 days.	Will provide the 150 day response.	NRC ltr. dtd. 10/27/81 acknowledged receipt of ltr. dt. 10/20/81 and requested licensee to submit weld material data as soon possible as a supplement to ltr. dtd. 10/20/81.
Turkey Point 4 (September 23, 1981)	Will provide responses within 30 days.	Will provide responses within 60 days.	Response will be delayed until March 1, 1982.	Information has been provided in 5/14/81 ltr. Will provide additional information with response to (3)	Will be answered in 60 days.	Will provide response Jan. 1, 1982. Will provide schedules & additional analyses for remedial actions by 3/1/82.	NRC letter dated 10/22/81 indicated staff would accept what licensee will provide. Staff will continue effort which may result in specifying conservatisms where more definitive info. is not available.
Robinson 2 (September 21, 1981)	Will provide responses within 60 days.	Will provide responses within 60 days.	Response will be deferred until 150 day response.	Information has been provided, however additional information will be provided in 150 day response.	Will be answered in in 60 days.	Will provide 150 day response.	NRC letter dated 10/22/81 indicated staff would accept what licensee will provide. Staff will continue effort which may result in specifying conservatisms where more definitive info. is not available.

Table A-1 (Continued)

Plant Name (Date of licensee's 30 day response to ltr of 8/21/01)	Licensee's 30 day response concerning 5 items to be submitted in 60 days					Licensee's 30 day response concerning the 150 day response.	Remarks
	(1) Request for present RT_{NDT} plates and welds	(2) Rate of RT_{NDT} increase	(3) RT_{NDT} limit for continued operation	(4) Basis for RT_{NDT} limit	(5) Questions concerning operator actions		
San Onofre 1 (October 5, 1981)	Will be provided by November 4, 1981 (15 day delay).	Will be provided by November 4, 1981 (15 day delay).	Will be provided as soon as made available. Owners Group work will not be complete until end of year.	Will be provided by November 4, 1981 (15 day delay).	Will be provided by November 4, 1981.	No conflict seen.	NRC letter dated 10/22/81 indicated staff would accept what licensee will provide. Staff will continue effort which may result in specifying conservatisms where more definitive info. is not available.
Oconee 1 (No 30 day response expected - telephone conversation indicated no conflicts seen).	Will provide 60 day response.	Will provide 60 response.	Will provide 60 day response.	Will provide 60 day response.	Will provide 60 day response.	Will provide 150 day response.	NRC ltr. dtd. 10/22/81 confirms that licensee anticipates no conflicts in providing 60 day and 150 day response.
TMI-1 (October 1, 1981)	Will provide response within 60 days.	Will be answered within 60 days.	Will be answered within 60 days.	Will be answered within 60 days.	Will be answered within 60 days.	Intend to submit response following plant-specific analysis established by B&W OG (Oconee 1-Dec. 31, 1981, Rancho Seco-Mar. 1, 1982. Others-later).	NRC ltr. dtd. 10/22/81 indicated that the staff will use Oconee 1 data as it is applicable to TMI-1. Staff encourages licensee to submit info. as it becomes available. Remainder of ltr. similar to to response for Calvert Cliffs 1.
Maine Yankee) (September 29, 1981)	Could answer within 60 days, however, would prefer to respond more fully within a short time after 60 day response is due.	Could answer within 60 days, however, would prefer to respond more fully within a short time after 60 day is due.	Could answer within 60 days, however, would prefer to respond more fully within a short time after 60 day resp. is due.	Could answer within 60 days, however, would prefer to respond more fully within a short time after 60 day resp. is due.	Could answer within 60 days, however, would prefer to respond more fully within a short time after 60 day resp. is due.	Will respond to II.K.2.13 by mid June 1982. More detailed fracture mechanics data available in 1982.	NRC ltr. dtd. 10/23/81 indicated staff would favor the response according to the licensee's licensee's proposed schedule. The lack of timely information on requested information could result in conservatisms in the staff conclusions.

Table A-2 Summary of 60 day responses to NRC letters dated August 21, 1981, concerning thermal shock to RPV
November 10, 1981

Plant Name (Date of 60 day response)	(1) Initial & Present RT_{NDT} for plates & welds	(2) Rate of RT_{NDT} increase continued oper.	(3) RT_{NDT} limit for	(4) Basis for RT_{NDT} limit	(5) Response concerning operator actions	Remarks
Calvert Cliffs 1 (10/20/81)	Limiting RT_{NDT} Values Initial Values Plate 20° Welds ° circ. -20° long. 10° @ 4.77 EFPY (12/31/81) Plate 92° Welds circ. 146° long. 178° Peak ID Fluence $7.05x10^{18}$ n/cm²	Licensee provided RT_{NDT} values for 7.97 EPFY (12/31/85) in resp. Plate 115° Welds circ. 194° long. 235° Peak ID Fluence $1.18x10^{19}$ n/cm²	Licensee does not consider it appropriate to define an upper limit RT_{NDT} value.	Adoption of RT_{NDT} limit would not permit consideration of warm prestressing or other factors.	Operators can control feed rate and terminate HPSI to overpressurization. Generic program will review procedures after detailed analyses of transients.	Licensee RT_{NDT} values are well within the staff's estimate.

Table A-2 (Continued)

Plant Name (Date of 60 day response)	(1) Initial & Present RT_{NDT} for plates & welds	(2) Rate of RT_{NDT} increase continued oper.	(3) RT_{NDT} limit for	(4) Basis for RT_{NDT} limit	(5) Response concerning operator actions	Remarks
Fort Calhoun (10/20/81)	Limiting RT_{NDT} Values Initial Values Plate 10° Welds circ. -20° long. -20° @ 5.36 EFPY (12/31/81) Plate 112° Welds circ. 245° long. 255° Peak ID Fluence - 7.04×10^{18} n/cm^2 RT_{NDT} values are based on generic material properties. Properties for archive material will be provided in 150 day response.	Licensee provided RT_{NDT} values for 8.58 EFPY (12/31/85) in response. Plate 142° Welds circ. 270° long. 268° Peak ID fluence - 1.12×10^{19} n/cm^2	Licensee does not consider it appropriate to define an upper limit RT_{NDT} value.	Adoption of RT_{NDT} limit would not permit considerations of warm prestressing.	HPSI throttling and termination and feedwater throttling criteria are provided in emergency transient procedures to prevent repressurization. CE will review procedures & where warranted procedure revisions will be proposed and evaluated. Following this, procedures will be changed and operating staff retrained as necessary.	Licensees RT_{NDT} values are within the estimates. By letter dtd. 10/23/81 the requested properties for archive material as soon as it is available as a supplement to to the 60 day resp.

Table A-2 (Continued)

Plant Name (Date of 60 day response)	(1) Initial & Present RT_{NDT} for plates & welds	(2) Rate of RT_{NDT} increase continued oper.	(3) RT_{NDT} limit for	(4) Basis for RT_{NDT} limit	(5) Response concerning operator actions	Remarks
Maine Yankee (11/2/81)	Limiting RT_{NDT} Values Initial Values Plate -10° Weld -30° As of 9/30/81 - (43 x 10^6 MWH electric RT_{NDT} @ ID = 180° Peak ID Fluence 5.4×10^{18} n/cm²	Licensee provided RT_{NDT} values for 26 more calendar years & end of life (35 total calendar years) for welds. 26 more cal. yrs 300° End of life 295°	Licensee does not consider it appropriate to define an upper limit RT_{NDT} value.	The program the licensee is working on considers the many variables involved in the vessel capabilities.	Operators are instructed with procedures to limit repressurization that results frm HPSI operation and removing RC pump operation during transients. Licensee maintains these actions contribute to problem.	Licensees RT_{NDT} values are well within the staff's estimates.
Turkey Point 4 (10/21/81)	Limiting RT_{NDT} Values Initial Values Forging 50° Circ. Welds 3° Current Values 5.61 EFPY (9/30/81 Forgings 85° Circ. Welds 193° Peak ID Fluence - 1.1×10^{19} n/cm² Fluence @ 1/4 T 6.6×10^{18} n/cm²	~~7°~~ 9°/EFPY for next 10 yrs 5°/EFPY for remaining life. For forgings, this represents 30° inc. for remaining design life of vessel.	Licensee stated in letter dtd. 9/23/81 that response will be delayed until 3/1/82.	Licensee stated in letter dtd. 9/23/81 information has been provided in 5/14/81 ltr. Will provide additional information with response to (3).	Licensee indicated no operator action is required for LOCA. Operator action is required within 10 min. for large MSLB. This includes criteria in procedures for HPSI termination and throttling AFW. LOCA procedures have similar procedures. Operators are trained in procedures and on simulator.	Licensees RT_{NDT} values are well within the staff's estimates.

Table A-2 (Continued)

Plant Name (Date of 60 day response)	(1) Initial & Present RT_{NDT} for plates & welds	(2) Rate of RT_{NDT} increase continued oper.	(3) RT_{NDT} limit for	(4) Basis for RT_{NDT} limit	(5) Response concerning operator actions	Remarks
Robinson 2 (10/26/81)	Limiting RT_{NDT} Values Initial Values Plate 46° @ 1/4 T Welds 0° Current Values 10 Plate 124° 1/4 T Plate 113° ID Weld 242° 1/4 T Weld 210° Fluence @ ID Plate 1.42×10^{19} n/cm^2 Fluence @ ID Weld 1.30×10^{19} n/cm^2	7°/EFPY for next 10 yrs 5°/EFPY for remaining life. 45° total for plate.	Licensee stated in ltr. dtd. 9/21/81 that responses will be deferred until 150 day response.	Licensee stated in ltr. dtd. 9/21/81 that information has been provided; however, additional information will be provided in 150 day repsonse.	Operators are provided in procedures HPSI termination criteria nd FW throttling criteria. HPSI pumps have 1500 psi shutoff heads. Training programs are established.	Licensees RT_{NDT} values are within the estimates.
San Onofre (11/ /81)	Limiting RT_{NDT} Values Initial Values Plate 60° Weld (long.) 0° Current Values @ 8.93 EFPY (10/31/81) Plate 222° Weld (long.) 229°	For Plate 4°/EFPY For Welds 3°/EFPY	Licensee stated that response will be provided upon completion of W Owners Group work.	RT_{NDT} should not be used as sole parameter to determine vessel integrity. Such a limit should be qualified to the specific method of calculation. Refers to Owenrs Group report of 5/14/81.	Existing procedures provide HPSI termination criteria for LOCA and SLB. Provides no provisions for throttling HPSI. Provided instruction to throttle feedwater for SLB operator action not required before 10 min. Training programs are provided. HPSI shutoff head is 1160 psi.	Licensees RT_{NDT} values are well within the staff's estimates.

Table A-2 (Continued)

Plant Name (Date of 60 day response)	(1) Initial & Present RT_{NDT} for plates & welds	(2) Rate of RT_{NDT} increase continued oper.	(3) RT_{NDT} limit for	(4) Basis for RT_{NDT} limit	(5) Response concerning operator actions	Remarks
Oconee 1 (10/20/81)	Limiting RT_{NDT} Values Initial Values Plate @ Nozzle 60° Weld circ. 20° long. 20° Current Values 5.13 EFPY (10/1/81) Plate 89° Weld circ. 145° long. 160° Plate Fluence 1.94×10^{18} n/cm² Weld Fluence 2.27×10^{18} n/cm²	Licensee provided fluence rate of increase for peak fluence and for critical weld location. Peak Fluence Rate - 0.37×10^{18} n/cm²/EFPY Weld Fluence Rate - 0.33×10^{18} n/cm²/EFPY	Licensee does not consider it appropriate to establish an upper limit RT_{NDT} value.	A RT_{NDT} limit would not provide confidence to predict toughness of materials and assurance that material with the greatest index is the controlling material for a given analysis.	Emergency procedures require operator action for controlling steam line break (overcooling) and LOCA. These include throttling and termination criteria for HPSI. The operator can take manual control of feedwater systems to limit plant cooldown.	The licensee's RT_{NDT} values are within the staff's estimates.

Table A-2 (Continued)

Plant Name (Date of 60 day response)	(1) Initial & Present RT_{NDT} for plates & welds	(2) Rate of RT_{NDT} increase continued oper.	(3) RT_{NDT} limit for	(4) Basis for RT_{NDT} limit	(5) Response concerning operator actions	Remarks
Three Mile Island 1 (10/23/81)	Limiting RT_{NDT} Values Initial Values Plate @ 1/4 T 40° Welds @ 1/4 T 20° Current Values Plate 83° Weld circ. 177° long. 170° Fluence for Plate 2.3×10^{18} n/cm^2 Fluence for circ. welds 2.1×10^{18} n/cm^2 Fluence for long. welds 1.7×10^{18} n/cm^2	For Plate 6.2° RT_{NDT}/EFPY For Circ Welds 22.8° RT_{NDT}/EFPY For Long. Welds 19.9 RT_{NDT}/EFPY These are the current rates. As plant life increases da/dt decreases.	Use of RT_{NDT} as a limiting parameter for continued operation is not considered appropriate by licensee.	The owners group will establish a set of parameters that are expected to be other than RT_{NDT}.	B&W Report BAW 1648 Guidelines have been incorporated in TMI-1 Emergency Procedures. For SBLOCA procedures provide for HPSI throttling (termination) criteria and feedwater control criteria. Training on these procedures is a part of operator training and retraining program.	The licensee's RT_{NDT} values for longitudinal welds are slightly higher than the staff's estimates (10°).

Table A-3 Summary of "150 day" responses concerning PTS

Plant (NSSS vendors)	Response to letters and gen. contents	Conclusions	Limiting transients	Criteria of accept.	Warm prestressing considered	Operator actions considered	Remedial actions
Robinson 2 1/25/82 (W) Referenced WCAP 10019	1. Irradiation data 2. Weld material info. 3. Basis for continued operation 4. Operator actions 5. Remedial actions	31 cal. yrs. of vessel life remaining for all transients considered.	Rpt. provided a table transients considered which include following: 1. Large LOCA 2. SBLOCA 3. LSLB 4. SSLB 5. Rancho Seco	Min. flaw depth for crack initiation is greater than 1.0 in. Crack arrest occurs within 75% of vessel	Yes, for all transients considered.	Refers to WCAP 10019. Credit is taken for LSLB. AFW terminated HPSI terminated in 10 min.	1. Will have low leakage core 2. Will keep abreast on annealing developments. 3. Studying benefits of heating RWST. 4. Verification analysis by EPRI.
Turkey Pt. 4 1/21/82 (W) Referenced WCAP 10019	1. Irradiation information 2. Weld material info 3. Transient fracture analysis showing basis for continued operation.	Reactor vessel integrity will be maintained throughout design life.	Rpt. provided a table of transients considered which include following: 1. Large LOCA 2. SBLOCA 3. LSLB 4. SSLB 5. Rancho Seco	Min. flaw depth for crack initiation is greater than 1.0". Crack arrest occurs within 75% of vessel wall thick.	Yes, all transients except SSLB	Cannot determine but WCAP 10019 provides following: Control AFW	Since integrity has been demonstrated, no need for action plan.

Table A-3 (Continued)

Plant (NSSS vendors)	Response to letters and gen. contents	Conclusions	Limiting transients	Criteria of accept.	Warm prestressing considered	Operator actions considered	Remedial actions
San Onofre 1/25/82 (W) Referenced WCAP 10019	1. Irradiation effects 2. Material property info 3. Basis for continued operation 4. Operation actions 5. Remedial Actions	Reactor vessel integrity will be maintained beyond design lifetime.	Rpt. provided a table of transients considered which include following: 1. Large LOCA 2. SBLOCA 3. LSLB 4. SSLB 5. Rancho Seco	Min. flaw depth for crack initiation is greater than 1.0". Crack arrest occurs within 75% of vessel wall thick.	Yes, for large and small LOCAs only.	<u>For LSLB</u> Terminate HPSI Terminate AFW to faulted SG. <u>For SSLB</u> Isolate break (PORV) Terminate HPSI	Plan for remedial actions not warranted. Low leakage core is place.
Ft. Calhoun 1/18/82 (CE)	1. Thermal-Hydro Eval. (a) SLB (b) Overcooling (antipated occurences) 2. Fracture Mech Analy. for SLB 3. Response to Dec. 18 ltr. 4. Fluence data	Integrity will be maintained for lifetime of plant.	HSLB most limiting. Overcooling AOO-stuck open dump valve. (SBLOCA + LOFW analyzed in CEN 189)	For MSLB (low probability) - crack arrest. For AOO + Single failure - crack arrest For AOO - no crack initiation	Benefit from W.P. not considered, however, it was not needed. It would have been credited if needed and criteria met.	Yes For MSLB - 30 min. to reduce HPSI flow. For MSLB - trip RC pumps in 30 seconds. For AOO trip RCP in 10 min. Reduce HPSI in 90 min.	1. Will implement reduced radial leakage fuel scheme in Cycle B. 2. Will study other fuel arrangement schemes 3. Do not plan increase in ECC water temp. 4. Evaluating annealing. 5. Program plan will evaluate control systems, procedures & potential design mods.

Table A-3 (Continued)

Plant (NSSS vendors)	Response to letters and gen. contents	Conclusions	Limiting transients	Criteria of accept.	Warm prestressing considered	Operator actions considered	Remedial actions
Maine Yankee 1/21/82 (CE)	APP A - response to 4-150 day questions APP B - response to RFI of 8/21/81 APP C - response to 12/18/81 ltr. May do further RETRAN analyses *Don't address selection of events causing highest PTS risk.	Vessel will retain integrity throughout design life.	MSLB most limiting (cooldown below 300°)	No crack initiation. Response references CEN 189 Report. Prob. of MSLB is very low.	Benefit from W.P. not considered, however, it was not needed. It would have credited if needed and criteria met.	Yes For MSLB Trip RCP @ 30 sec. Terminate HPSI @ 30 min. For AOO Trip RCP @ 10 min. Terminate HPSI @ 90 min.	1. Low leakage fuel mgmt. for Cycle 7. 2. Will operate RWS to maintain higher temp. not to exceed 80° 3. Will keep informed on annnealing. 4. Will evaluate control strategy after plant-specific evaluation is in place.
Calvert Cliffs 1/28/82 (Resp. to 12/18/81 ltr 1/21/82) (CE)	1. Was responsive 2. Fluence cal. 3. Systems Analysis 4. Fracture mechanics	No crack initiation for assumed plant life for SBLOCA + LOFW. Same for stuck open dump valve (AOO) For MSLB, satisfactory performance for 21 add'l EFPY	MSLB most limiting, AOO + single failure. SBLOCA + LOFW analysis in CE 189	No crack initiation for AOO Crack arrest for MSLB	Benefit from W.P. not considered, however, it was not needed. It would have credited if needed and criteria met.	Yes For MSLB Trip RCP @ 30 sec. Reduce HPSI flow @ 30 min. For AOO Trip RCP @ 10 min. Terminate AFW @ 10 min. Reduce HPSI @ 90 min.	1. Scoping studies on fuel mgmt. 2. Do not plan to increase RWST temperature. 3. No discussion on annealing. 4. Control system changes may be considered.

Table A-3 (Continued)

Plant (NSSS vendors)	Response to letters and gen. contents	Conclusions	Limiting transients	Criteria of accept.	Warm prestressing considered	Operator actions considered	Remedial actions
Oconee 1 1/15/82 (B&W)	1. Overcooling transient analysis** 2. SBLOCA analysis 3. Mixing analysis 4. Vessel wall thermal analysis 5. Material properties 6. [illegible] determination 7. [illegible] mechanics analysis 8. Frequency determination 9. SLB analysis **Turbine bypass system failures, overfill transients	Vessel failure is not calculated to result from postulated transient. With minimal downcomer mixing, no credit for mixing in hot leg, no credit for W.P.-16 EFPY. With credit for W.P., for SBLOCA - 32 EFPY. For overcooling transient 25 EFPY (Design life - approx. 27 EFPY)	SBLOCA + LOFW Overcooling transient	Crack initiation with arrest within 1/4 T	Yes, for SBLOCA. No for overcooling transient	<u>SBLOCA + LOFW</u> Trip RCP. Throttle HPIS @ 93 min. <u>Overcooling Transient</u> Trip RCP. Isolate EFWS @ 20 min. Except MSLB - isolate all feedwater within 5 min. Only assumed above actions where necessary to mitigate consequences and achieve acceptable EFPY.	1. 18 month fuel cycle provides decrease in leakage flux. 2. Current water temperature sufficient. 3. In-place annealing not required. 4. No control system changes are necessary.

Table A-4 Summary of generic reports concerning PTS

Owners Group	General Contents	Conclusions	Limiting Transients	Criteria of Acceptance	Warm Prestressing Considered	Operator Action Consider	Potential Remedial Action
Westinghouse WCAP 10019 December 1981 "Summary Report on Reactor Vessel Integrity for Westinghouse Operating Plants"	1. Limited transient development 2. Fluence Calc. 3. Stress & Fracture Mechanics for Transients 4. Vessel Integrity Evaluations 5. Potential Remedial Actions 6. Conclusions (for each operating plant) 7. Don't address idenfication of events causing highest PTS risk	All plants can continue operation a number of yrs (3 for the least) before acceptance criteria is violated. A table provides no. of yrs. for each plant. Eight plants are 5 yrs or less	**1. Small Steam Line Break 2. Rancho Seco 3. Large Steam Line Break 4. Small LOCA 5. Large LOCA *In order of severity. **Most limiting.	1. No initiation of flaws less than 1 inch deep. (Flaws > 1 in. deep not assumed to exist) or 2. Crack Arrest occurs within 75% of wall thickness.	Benefit of W.P. considered for SBLOCA and some large LOCA and large SL breaks. Benefit was not considered for other transients.	Yes-Control AFW Trip RCPs as examples - Rept. is not very definitive.	1. Heating RWST to 80° - provide of 3 to 30 EFPY operation. 2. Limit AFW 3. Control Systems to mitigate transients a. RC Press. Relief System b. Safety Injection Control c. AFW Control 4. Core Modifications a. Low leakage loading 5. Annealing Vessel a. Is feasible

Table A-4 (Continued)

Owners Group	General Contents	Conclusions	Limiting Transients	Criteria of Acceptance	Warm Prestressing Considered	Operator Action Consider	Potential Remedial Action
Combustion <u>Engineering</u> CEN-189 "Evaluation of Pressurized Thermal Shock Effects Due to Small Break LOCAs with Loss of Feedwater for CE NSSS"* December 1981 *(This is the Post-TMI "feed & bleed" rept. It is not a Generic PTS report.)	1. Only addresses SBLOCA with loss of all FW transient 2. Thermal Hydro-analysis 3. Discussions on mixing Additional studies are expected to permit removal of certain conservatisms 4. Scoping studies indicate range of HPSI flows must be considered 5. Fluence Calculations 6. Material Properties 7. Vessel Integrity Evaluation 8. Plant-Specific Analysis	Each plant's vessel can safely withstand SBLOCA + LOFW for design life without crack initiation.	1. Only considers SBLOCA + LOFW **(Note that MSL break is most limiting but was only considered in the 150 day responses)	1. No initiation of flaws of credible size, or if it does initiate. 2. Arrest after limited extension.	Benefit of WP was considered	Yes: 1. PORVs opened in 10 min. 2. AFW reinstated after 30 min.	None considered.
No B&W Report -- promised plant-specific analyses. No generic report promised.							

REFERENCES

1. NRC Memorandum dated April 7, 1981, from T. J. Walker to S. S. Pawlicki - Minutes of PWR Owners Groups Meeting with NRC on March 31, 1981.

2. NRC Letters dated April 20, 1981 from D. G. Eisenhut to Licensees of Operating PWR Nuclear Power Plants - Thermal Shock to Reactor Pressure Vessels (Generic Letter 81-19).

3. Summary of Meetings with the Babcock & Wilcox, Westinghouse, and Combustion Engineering Owners Groups on July 28, 29 and 30, 1981, Respectively, Concerning Pressurized Thermal Shock to Reactor Pressure Vessels - dated August 18, 1981.

4. Summary of Meeting with the Westinghouse Owners Group of September 18, 1981 Concerning Pressurized Thermal Shock to Reactor Pessure Vessels - dated October 1, 1981.

5. Summary of Meeting with the B&W Owners Group on September 22, 1981 Concerning Pressurized Thermal Shock to Reactor Pressure Vessels - dated October 1, 1981.

6. Summary of Meeting with the Combustion Engineering Owners Group on October 7, 1981 Concerning Pressurized Thermal Shock to Reactor Pressure Vessels - Dated October 21, 1981.

7. Summary of Meeting with the Westinghouse Owners Group and the Three Selected Owners on February 24, 1982 Concerning the Pressurized Thermal Shock Issue - dated March 8, 1982.

8. Summary of Meeting with Combustion Engineering Owners Group and the Three Selected Owners of CE Designed Plants on March 3, 1982 Concerning the Pressurized Thermal Shock Issue - dated March 12, 1982.

9. Summary of Meeting with Duke Power Company on March 24, 1982 Concerning the Pressurized Thermal Shock Issue for Oconee Unit No. 1 - dated October 31, 1981.

10. Summary of Meeting with Omaha Public Power District on May 6, 1982 Concerning the Pressurized Thermal Shock Issue - dated May 13, 1982.

11. Summary of Meeting with the Westinghouse Owners Group on May 10, 1982 Concerning the Pressurized Thermal Shock Issue - dated May 21, 1982.

12. Summary of Meeting with GPU Nuclear on June 2, 1982 Concerning the PTS issue for TMI-1 - dated June 16, 1982.

13. Summary of Meeting with PWR Industry Representatives on June 9, 1982 Concerning the PTS issue.

14. Summary of Meeting with WOG on Reactor Vessel Integrity on June 22, 1982 Concerning the PTS issue - dated June 30, 1982.

15. Summary of Meeting with CEOG on June 23, 1982 Concerning the PTS issue - dated July 8, 1982.

16. Summary of Meeting with tne WOG on July 28, 1982 Concerning the PTS issued - dated ____________.

17. NRC Letters dated August 21, 1981, from Darrell G. Eisenhut to eight selected utilities (Florida Power & Light Company, Carolina Power & Light Company, Southern California Edison Company, Baltimore Gas & Electric Company, Omaha Public Power District, Maine Yankee Atomic Power Company, Duke Power Company and GPU Nuclear Corporation) Concerning Pressurized Thermal Shock to Reactor Pressure Vessels.

18. NRC Letter dated October 2, 1981 from Mr. T. M. Novak, NRC, to Mr. A. E. Lundvall, Jr., Baltimore Gas & Electric Company, Concerning Responses to the NRC August 21, 1981 Letter.

19. NRC Letter dated October 26, 1982, from Mr. T. M. Novak, NRC, to Mr. W. C. Jones, Omaha Public Power District, Concerning Responses to NRC Letter dated August 21, 1981.

20. NRC Letter dated October 23, 1981 from Mr. T. M. Novak, NRC, to Mr. Robert H. Groce, Maine Yankee Atomic Power Company, Concerning Responses to NRC Letter dated August 21, 1981.

21. NRC Letter dated October 26, 1981 from Mr. T. M. Novak, NRC, to Mr. J. A. Jones, Carolina Power & Light Company, Concerning Responses to NRC Letter dated August 21, 1981.

22. NRC letter dated October 26, 1981 from Mr. T. M. Novak, NRC, to Dr. Robert E. Uhrig, Florida Power & Light Company, Concerning Responses to NRC Letter dated August 21, 1981.

23. NRC Letter dated October 23, 1981 from Mr. Gus C. Lainas, NRC, to Mr. R. Dietch, Southern California Edison Company, Concerning Responses to NRC Letter dated August 21, 1981.

24. NRC Letter dated October 23, 1982 from Mr. T. M. Novak, NRC, to Mr. William O. Parker, Jr., Duke Power Company, Concerning Responses to NRC Letteer dated August 21, 1981.

25. NRC Letter dated October 23, 1981 from Mr. T. M. Novak, NRC, to Mr. Henry D. Hukill, Metropolitan Edison Company, Concerning Responses to the NRC Letter dated August 21, 1981.

26.-
3: RC Letters dated December 18, 1981, to Maine Yankee Atomic Power Company, Baltimore Gas & Electric Company, Omaha Public Power District, Carolina Power & Light Company, Florida Power & Light Company, Southern California Edison Company, Duke Power Company, and Metropolitan Edison Company - Provided Evaluations of the "60 day" Responses to the NRC Letter dated August 21, 1981, and Requested Additional Information to be Provided in the "150 day" Responses.

34. NRC Letter dated March 15, 1982 to Southern California Edison Company Concerning Request for Information Related to their "150 day" Response.

35. NRC Letter dated March 16, 1982, to Carolina Power & Light Company Concerning Request for Information Related to their "150 day" Response.

36. NRC Letter dated March 16, 1982, to Florida Power & Light Company Concerning Request for Information Related to their "150 day" Response.

37. NRC Letter dated March 16, 1982 to Mr. Oliver Kinglsey, Chairman of WOG, Concerning Request for Information Related to W Generic Program on PTS.

38.-
40. NRC Letters dated March 18, 1982 to Maine Yankee Atomic Power Company, Omaha Public Power District and Baltimore Gas & Electric Company Concerning Requests for Information Related to their "150 day" Responses.

41. NRC Letter dated April 4, 1982 to Duke Power Company Concerning Request for Information Related to their "150 day" Responses.

42. B&WOG Letter dated May 12, 1981 from John J. Mattimoe, Chairman B&WOG, to Harold Denton, NRC, Concerning Report on Reactor Vessel Brittle Fracture Concerns in B&W Operating Plants.

43. CEOG Letter dated May 15, 1981 from Mr. K. P. Baskin, Chairman CEOG, to Mr. Darrell G. Eisenhut, NRC, Concerning Reactor Vessel Pressurized Thermal Shock.

44. WOG Letter dated May 14, 1981 from Mr. Robert W. Jurgensen, Chairman OG, to Mr. D. G. Eisenhut, NRC, Concerning Thermal Shock to Reactor Pressure Vessel.

45. Baltimore Gas & Electric Company Letter dated September 24, 1981 to NRC Concerning 30 day Response for Calvert Cliffs 1 to August 21, 1981 Letter.

46. Omaha Public Power District Letter dated September 22, 1981 to NRC Concerning 30 day Response to August 21, 1981 Letter for Fort Calhoun.

47. Maine Yankee Atomic Power Company Letter dated September 29, 1981 to NRC Concerning 30 day Response to August 21, 1981 Letter for Maine Yankee.

48. Florida Power & Light Company Letter dated September 23, 1981 to NRC Concerning 30 day Response to August 21, 1981 Letter for Turkey Point 4.

49. Carolina Power & Light Company Letter dated September 21, 1981 to NRC Concerning 30 day Response to August 21, 1981 Letter for Robinson 2.

50. Southern California Edison Company Letter dated October 5, 1981 to NRC Concerning 30 day Response to August 21, 1981 Letter for San Onofre 1.

51. Metropolitan Edison Letter dated October 1, 1981 to NRC Concerning 30 day Response to August 21, 1981 Letter for TMI-1.

52. Baltimore Gas & Electric Company Letter dated October 20, 1981 to NRC Concerning 60 day Response to August 21, 1981 Letter for Calvert Cliffs 1.

53. Omaha Public Power District Letters dated October 20 and November 12 and 13, 1981, to NRC Concerning 60 day Response to August 21, 1981 Letter for Fort Calhoun.

54. Maine Yankee Atomic Power Company Letter dated November 2, 1981 to NRC Concerning 60 day Response to August 21, 1981 Letter for Maine Yankee.

55. Florida Power & Light Company Letter dated October 21, 1981 to NRC Concerning 60 day Response to August 21, 1981 Letter for Turkey Point 4.

56. Carolina Power & Light Company Letter dated October 26, 1981 to NRC Concerning 60 day Response to August 21, 1981 Letter.

57. Southern California Edison Company Letter dated November 4, 1981 to NRC Concerning 60 day Response to August 21, 1981 Letter for San Onofre 1.

58. Duke Power Company Letter dated October 20, 1981 to NRC Concerning 60 day Response to August 21, 1981 Letter for Oconee 1.

59. Metropolitan Edison Company Letter dated October 23, 1981, to NRC Concerning 60 day Response to August 21, 1981 Letter for TMI-1.

60. Baltimore Gas & Electric Company Letters dated January 21 and 28, 1982 to NRC Concerning Request for Information dated December 18, 1981 and 150 day Response to August 21, 1981 Letter, Respectively, for Calvert Cliffs 1.

61. Omaha Public Power District Letter dated January 18, 1982 to NRC Concerning 150 day Response to August 21, 1981 Letter for Fort Calhoun.

62. Maine Yankee Atomic Power Company Letter dated January 21, 1982 to NRC Concerning 150 day Response to August 21, 1981 Letter for Maine Yankee.

63. Carolina Power & Light Company Letter dated January 25, 1982 to NRC Concerning 150 day Response to August 21, 1981 Letter for Robinson 2.

64. Florida Power & Light Company Letter dated January 21, 1982 to NRC Concerning 150 day Response to August 21, 1981 Letter for Turkey Point 4.

65. Southern California Edison Company Letter dated January 25, 1982 to NRC Concerning 150 day Response to August 21, 1981 Letter for San Onofre 1.

66. Duke Power Company Letter dated January 15, 1982 to NRC Concerning 150 day Response to August 21, 1981 Letter for Oconee 1.

67. WOG Letter dated December 30, 1981 to NRC from O. D. Kinglsey, Chairman WOG, Concerning WCAP-10019 "Summary Report on Reactor Vessel Integrity for Westinghouse Operating Plants."

68. CEOG Letter dated December 32, 1981 to NRC from K. P. Baskin, Chairman CEOG, Concerning CEN-189 "Evaluation of Pressurized Thermal Shock Effects Due to Small Break LOCAs with Loss of Feedwater for Combustion Engineering NSSS."

69. WOG Letter dated May 28, 1982 to NRC from Mr. O. D. Kinglsey, Chairman WOG, Concerning Response to Request of NRC Letter dated March 16, 1982.

70. Carolina Power & Light Company Letter dated May 4, 1982 Concerning the NRC Requests for Information dated March 16 and April 20, 1982.

71. Florida Power & Light Company Letter dated May 3, 1982 to NRC Concerning Response to NRC Request for Information dated March 16, 1982.

72. Southern California Edison Company Letter dated May 25, 1982 to NRC Concerning Response to NRC Request for Information dated March 16, 1982.

73. WOG LEtter dated June 16, 1982 from O. D. Kingsley, Chairman WOG, to H. R. Denton, NRC, Concerning Report "Fuel Management to Reduce Neutron Flux."

74. WOG Letter dated June 22, 1982, from O. D. Kinglsey, Chairman WOG, to H. R. Denton, NRC, transmitting report "Review of Emergency Response Guidelines Relative to PTS."

75. Baltimore Gas & Electric Company Letter dated May 4, 1982 to NRC in Response to NRC Request for Information dated March 18, 1982.

76. Omaha Public Power District Letter dated April 30, 1982 to NRC in Response to NRC Request for Information dated March 18, 1982.

77. Maine Yankee Atomic Power Company Letter dated May 11, 1982 to NRC in Response to NRC Request for Information dated March 18, 1982.

78. CEOG Letter dated June 14, 1982 from Ken Baskin, Chairman CEOG, to H. R. Denton, NRC, Concerning the Proposed NRC recommendations on the PTS issue.

79. Omaha Public Power District Letter dated June 28, 1982 to NRC as a result of the meeting of June 23, 1982 with the staff.

80. Duke Power Company Letter dated April 30, 1982 in Response to NRC Request for Information dated April 5, 1982.

81. GPU Nuclear Corporation Letter dated March 17, 1982 to NRC Concerning 150 day Response to August 21, 1981 Letter for TMI-1.

82. GPU Nuclear Letter dated June 1, 1982 to the NRC Concerning a summary of the "150 day" response to the August 21, 1981 letter and request for information letter dated December 18, 1981.

83. B&WOG Letter dated June 22, 1982 from A. P. Rochino, Chairman of B&WOG, to H. R. Denton, NRC, Concerning the staff proposed recommendations on the PTS issue.

84. Duke Power Company Letter dated June 21, 1982, Concerning the staff's proposed recommendations on the PTS issue.

85. Arkansas Power & Light Company Letter dated June 21, 1982 Concerning the staff's proposed recommendations on the PTS issue.

86. Florida Power Corporation Letter dated June 28, 1982 Concerning the staff's proposed recommendations on the PTS issue.

87. GPU Nuclear Letter dated July 7, 1982 Concerning the staff's proposed recommendations on the PTS issue.

88. SMUD Letter dated June 21, 1982 Concerning the staff's proposed recommendations on the PTS issue.

89. NRC Internal Memorandum from G. R. Mazetis, Chairman of Robinson PTS Task Force, to H. L. Thompson, Acting Director of DHFS, dated April 15, 1982 concerning staff audit of Robinson 2 procedures and training for PTS.

90.-

95. NRC Letters dated April 26, 22, 22, 22, 22 and 23 to Florida Power & Light Company, Maine Yankee Atomic Power Company, Baltimore Gas & Electric Company, Omaha Public Power District, Southern California Edison Company, and Duke Power Company requesting cooperation in the audit and evaluation effort on plant procedures and operator training related to PTS.

96. NRC Letter dated April 20, 1982 confirming the Carolina Power & Light Company's commitment to the recommendations of the NRC Robinson 2 Task Force on PTS.

97. Carolina Power & Light Company Letter dated May 4, 1982 confirming the utility's commitment to the recommendations of the NRC Robinson 2 Task Force on PTS and providing additional information which was requested in the NRC March 16, 1982 letter.

98. Pacific Northwest Laboratory's Letter dated June 4, 1982 Concerning the review of the procedures and training for PTS at Oconee 1.

99. NRC Summary of June 8-10, 1982 meeting with OPPD regarding the procedures and operator training relative to the PTS issue - dated June 16, 1982.

100. NRC meeting of July 30, 1982, with WOG regarding SBLOCA which result in stagnation flow, frequencies of such events and differences between staff and WOG concerning the fracture mechanics analyses dated August 9, 1982.

101. NRC Summary of August 11, 1982 meeting with WOG to resolve differences between the staff and WOG concerning the screening criteria dated August 20, 1982.

APPENDIX B

CHRONOLOGY OF EVENTS CONCERNING PRESSURIZED THERMAL SHOCK

December 31, 1980 B&W licensees submitted Thermal Mechanical Report (BAW-1648).

March 31, 1981 - NRC meeting with PWR Owners Groups concerning thermal shock with repressurization issue. Owners Groups committed to a report by May 15, 1981 to put thermal shock issue into perspective.

April 20, 1981 letter to all operating PWR licensees requesting Owners Groups Reports by May 15, 1981 and licensee's responses by May 22, 1981.

May 4, 1981 - Commission Information Paper (SECY-286 (2B).

June 11, 1981 - Commission Briefing.

May 12, 1981 - Board Notification.

May 15, 1981 - Received May 15 reports from Owners Groups.

May 19, 1981 - ACRS subcommittee meeting to discuss Owners Groups responses.

May 28-June 4 - Received responses from all operating PWR licensees.

June 5, 1981 - ACRS Briefing.

June 11, 1981 - Commission Briefing.

July 28, 29, 30, 1981 - Meetings with Babcock & Wilcox, Westinghouse and Combustion Engineering Owners Groups.

September 15, 1981 - Commission Briefing.

August 21, 1981 - NRC letter to eight licensees of eight operating PWR plants (Fort Calhoun, Robinson 2, San Onofre, Maine Yankee, Turkey Point 4, Calvert Cliffs 1, TMI-1 and Oconee 1) requesting 60-day response and 150-day response concerning Thermal Shock.

September 21 through October 5, 1981 - Received letters from 7 licensees in response to the August 21, 1981 letter identifying conflicts with the request.

September 18, 1981 - Meeting with Westinghouse Onwers Group.

September 22, 1981 - Meeting with Babcock & Wilcox Owners Group.

October 7, 1981 - Meeting with Combustion Engineering Owners Group.

October 23-28, 1981 - Letters to eight licensees regarding their exceptions to the August 21, 1981 letter.

October 20 through November 13, 1981 - "60 day" responses from the eight licensees who received the August 21, 1981 letter.

December 8, 1981 - Commission Paper SECY 81-687 dated December 8, 1981, Subject: Designation of PTS as an Unresolved Safety Issue.

December 18, 1981 - NRC evaluations and request for information concerning "60 day" response.

December 30, 1981 - Westinghouse Owners Group Report Concerning Pressure Vessel Integrity.

December 31, 1981 - Combustion Engineering Owners Group Report concerning TMI Action Item II.K.2.13.

January 15 through January 25, 1982 - "150 day" responses to August 21, 1981 letter from seven utilities. GPU did not submit a "150 day" response for TMI-1.

February 24, 1982 - Meeting with WOG to discuss the WOG generic report and the "150 day" responses of three W plants of PTS concern.

March 3, 1982 - Meeting with CEOG to discuss the CEOG generic report and the "150 day" responses of the three CE plants of PTS concern.

March 5, 1982 - Commission Information Paper (SECY 82-92) Subject: Commission Briefing on PTS.

March 9, 1982 - Commission Briefing, Status Report on PTS.

March 16, 1982 - Appointment of Special Task Groups to (1) investigate the reducing of irradiation damage to vessels, and (2) audit the operator training and procedures for the PTS concern at Robinson 2.

March 15, 16, 18 and 24, April 5, 1982 - Letters to seven of the eight licensees of the PTS concerned plants and the WOG requesting additional information related to the "150 day" responses and the generic reports.

March 24, 1982 - Meeting with Duke Power Company to discuss the Oconee 1 "150 day" response.

March 26, 1982 - Transmittal of Task Action Plan for USA A-49, "Pressurized Thermal Shock" (PTS).

March 24, 1982 - Meeting with Duke Power Company concerning the PTS issue for Oconee 1.

April 15, 1982 - Report of special task force on PTS for Robinson 2.

May 20, 1982 - Preliminary Assessment of Techniques for Fluence Rate Reduction for PWR Pressure Vessels.

April 30-May 4, 1982 - Received responses from licensees of special plants concerning PTS to NRC request for information during March 1982.

May 6, 1982 - Meeting with OPPD concerning PTS issue. Discussed OPPD's response of April 30, 1982.

May 10, 1982 - Meeting with WOG concerning the PTS issue. Discussed response of WOG due at end of May.

April 26, 1982 - Licensees of other six special plants of PTS concern requested to cooperate in audits of operating procedures and training.

May 28, 1982 - Received WOG Supplemental Information on Reactor Vessel Integrity.

June 2, 1982 - Meeting with GPU Nuclear concerning PTS, Summary of "150 day" response.

June 3, 1982 - ACRS Meeting - Discussed staff's Consideration of Possible Recommendations for PTS Requirements.

June 9, 1982 - Meeting with PWR industry representatives concerning the staff's considerations of possible recommendations for PTS requirements.

June 22, 1982 - Meeting with WOG concerning PTS issue - Followup to June 9, 1982 meeting.

June 23, 1982 - Meeting with CEOG concerning PTS issue - Followup to June 9, 1982 meeting.

June 16, 1982 - WOG report on Fuel Management to Reduce Neutron Flux.

June 22, 1982 - WOG report on PTS review of ERGs.

June 21-July 7, 1982 - Responses from CEOG, OPPD, B&WOG, and licensees of all operating B&W plants concerning staff's consideration of proposed recommendations on PTS requirements.

July 9, 1982 - Meeting with OPPD concerning PTS issue.

July 28, 1982 - Meeting with WOG concerning PTS issue - discussed staff's position on PTS issue.

APPENDIX C

PROCEDURES AND TRAINING

C.1 Human Factors Considerations

It was recognized by the task force early in the review of Pressurized Thermal Shock (PTS) that plant operators played a key role in the evaluation and mitigation of PTS events. There are some key considerations that must be evaluated in determining the acceptability of operator action as a mitigative action.

The first is that reactor vessels have been designed to withstand the worst design-basis accident. The consequences of a vessel failure are so significant that we have always required vessel and system design adequate to prevent it. The second consideration is a concern for the ability of the operators to 'prevent' PTS from breaking a vessel. Operators in general are excellent throughout the industry. But any human can make errors, both cognitive and operative. The likelihood of error increases with an increase in stress, poor control room design, fatigue, instructions inadequate to deal with the particular sequence in progress, and other similar factors. Because of possible human errors and the potential severe consequences of PTS, the NRC does not consider operator action an acceptable long-term "solution" to the PTS issue.

However, the NRC staff recognizes that there is a genuine need to provide clear, concise, and integrated procedures and training to the operators, to ensure they know the technical issues involved not only for this issue, but for other vital considerations they must be concerned with in plant operations. After the TMI-2 accident, NRC-directed 'enhancements' to HPI termination criteria were developed by the industry. The results of these changes is, as perceived by the NRC staff, a 'mindset' to maintain HPI flow after an accident at all costs. Current analysis of accidents with continuous HPI flow shows that the challenge to vessel integrity is more severe than previously considered. In subsequent evaluations, the staff and the industry have learned that real events, with multiple failures, have led to transient cooldowns more severe than previously analyzed. This led the staff to recognize that a balance of considerations must be used to control the operation of HPI and other safety-related plant equipment. The industry should have a clear understanding of those considerations, including understanding of PTS, in determining the best method of operating plant equipment.

C.1.1 Westinghouse Plants

The utilities of the three Westinghouse-designed plants being evaluated for PTS provided a list of procedural steps dealing with HPI termination and control of feedwater. These steps were provided in the 60-day responses to D. G. Eisenhut's August 21, 1981 letter to the eight plants being evaluated for PTS. In response to D. G. Eisenhut's December 18, 1981 letter to the three utilities with Westinghouse-designed plants, additional procedures information was provided at the same time the 150-day response to our August 21, 1981 letter were provided.

At a meeting in February 1982, in Bethesda, Md., Westinghouse presented to the NRC staff an evaluation of the PTS mitigative actions contained in the Westinghouse guidelines, which were developed in response to NUREG-0660 Item I.C.1. The guideline for steam line breaks includes modified HPI termination criteria, to account for vessel integrity considerations, as described in the following: The HPI termination criteria require a level in the steam generator, a level in the pressurizer, adequate subcooling margin, and a minimum pressure. For vessel integrity considerations, the minimum pressure for HPI termination has been lowered in the steam line break guideline from 2000 psig to 700 psig when primary loop temperature is below 350°F.

A letter from S. Varga to the three Westinghouse licensees dated March 16, 1982 requested evaluations regarding the need and effectiveness of upgrading current procedures, and requesting a formal commitment to upgrade operator understanding of PTS. Responses from Carolina Power and Light (H. B. Robinson), Florida Power and Light (Turkey Point 4) and Southern California Edison (San Onofre 1) dated May 4, 1982, May 3, 1982, and May 20, 1982, respectively, were received.

C.1.1.1 H. B. Robinson

C.1.1.1.1 Present Procedures

H. B. Robinson's emergency operating procedures were based on the Westinghouse guidelines developed in response to NUREG-0660 Item I.C.1. They include the modified HPI termination criteria for steam line breaks. CP&L stated in their 150-day response that they believe procedures governing operator action and programs governing operator training should provide a balanced approach to handling transients and accidents. Their heatup and cooldown curves are used to define acceptable operation to prevent PTS events. An additional training program on the recent PTS concerns was completed March 31, 1982. H. B. Robinson continues to tie their efforts into the Westinghouse procedures development effort. Modifications to the Robinson procedures are being made, as outlined in Section C.1.1.1.3.

C.1.1.1.2 Present Operator Training

As stated in the previous section, H. B. Robinson believes in a balanced approach to operator training. As described in their 150-day response, CP&L has committed to assuring that each of their operators has a complete understanding of the PTS issue. CP&L stated in a June 25, 1982 letter that operator training has been upgraded as outlined in the staff audit report.

C.1.1.1.3 Plant Audit

On April 5-7, 1982, the procedures and training related to PTS were audited at the site. The report of this audit is available separately. Some specific changes were recommended to the operating procedures to lower the required minimum pressure for HPI termination and to provide explicit instruction for pressure control during cooldown. More specific training was recommended, to include instruction on previous overcooling events, walk-throughs of procedures as a shift team, and CP&L evaluation of the shift's ability to cope with a PTS event. In a letter from E. Eury to T. Novak dated May 4, 1982, CP&L committed to address the staff's concerns, and identified other procedure modifications required as the result of Westinghouse's review of the guidelines on which the procedures are based.

C.1.1.2 Turkey Point 3

C.1.1.2.1 Present Procedures

Turkey Point Units 3 and 4 emergency operating procedures were developed based on the Westinghouse guidelines developed in response to NUREG-0660 Item I.C.1. As stated in the FP&L 150-day response, they include the modified HPI termination criteria for steam line breaks, and specific direction to terminate HPI when termination criteria are met. Operating pressure-temperature limit curves are included for use in operations. Emergency operating procedures provide instructions to (1) minimize RCS cooldown rate, and (2) prevent repressurization following overcooling. In a letter from R. Uhrig to S. Varga dated May 3, 1982, FP&L committed to modify their procedures based on the information provided in the staff's H. B. Robinson audit report.

Additionally, FP&L stated that other NRC concerns with existing procedures will be resolved in the guidelines (and subsequent procedures) developed in response to NUREG-0737 Item I.C.1. These procedures are to resolve NRC concerns both from a technical and human factors standpoint.

C.1.1.2.2. Present Operator Training

As stated in the FP&L 150-day response, pressure-temperature limit curves are presented and discussed in the Licensed Operator Training Program. Simulator training includes handling overcooling transients. In a letter from R. Uhrig to S. Varga dated May 3, 1982, FP&L stated they will be augmenting operator training based on the findings in the staff's H. B. Robinson audit report.

C.1.1.2.3 Plant Audit

On July 13-15, 1982, the procedures and training related to PTS were audited at the site. The report of this audit is available separately.

C.1.1.3 San Onofre 1

C.1.1.3.1 Present Procedures

San Onofre 1's emergency operating procedures were developed based on the Westinghouse guidelines developed in response to NUREG-0660 Item I.C.1. As stated in the SCE 60-day response, they include modified HPI termination criteria for steam line breaks, but do not provide specific direction to terminate HPI when termination criteria are met. In a recent procedure modification made for the Systematic Evaluation Program evaluation of steam line breaks, the operators are specifically directed to terminate HPI. Information obtained from the staff's H. B. Robinson audit report is also incorporated into the recently revised procedures.

C.1.1.3.2 Present Operator Training

As stated in their 150-day response, SCE provided formal operator training for PTS during the operator requalification training program conducted in February 1982. Recent format changes to procedures, modified HPI termination pressures, and upgraded knowledge of steam generator tube ruptures have recently been incorporated into the San Onofre 1 emergency operating procedures. These procedures changes will require additional training of the San Onofre 1 operators, to be conducted prior to startup from their current outage.

C.1.1.3.3 Plant Audit

An onsite audit was conducted of the San Onofre Unit 1 procedures and training for PTS June 2-4, 1982. Preliminary findings from the audit indicate that the procedures are based on plant-specific analyses of transients and that the operations personnel were familiar with PTS even though their training was not completed at the time of the audit. The findings indicatc that the remainder of the training program should include instruction on post-cooldown actions. Findings on the San Onofre 1 procedures are included in the audit report. The procedures were generally found adequate for PTS considerations, and were based on Westinghouse analysis. The findings indicate that a method for plotting cooldown rate shoul be provided to the operators.

C.1.2 Combustion Engineering Plants

The utilities of the three Combustion Engineering (CE) designed plants being evaluated for PTS provided a description of the procedural actions for dealing with HPI termination, and control of feedwater.

These steps were provided in the 60-day response to D. G. Eisenhut's December 18, 1981 letter to the three utilities with CE-designed plants. Additional procedures information was provided at the same time the 150-day responses to the August 21, 1981 letter were provided. At a March 1982 meeting in Bethesda, Md., licensee representatives of CE plants presented to the NRC staff an evaluation of the mitigative actions in the specific plant procedures. A letter from R. Clark to A. Lundvall, Jr., dated March 18, 1982, requested additional information regarding the basis and sensitivity of operator action assumed in the analyses performed for the 150-day responses. Responses from Omaha Public Power District (Fort Calhoun), Baltimore Gas and Electric (Calvert Cliffs) and Maine Yankee Atomic Power Company (Maine Yankee) were received by letters dated April 30, 1982, May 4, 1982, and May 11, 1982, respectively.

C.1.2.1 Fort Calhoun

C.1.2.1.1 Present Procedures

Fort Calhoun's emergency operating procedures cover design-basis events to cover the requirements for "10 CFR Part 50 Appendix B, Quality Assurance Criteria for Nuclear Power Plants and Fuel Reprocessing Plants." In OPPD's 60-day response, a detailed list of procedural actions was provided, including explanations of their applicability to PTS. In their 150-day response, OPPD stated that based on their own evaluation of their procedures, and on the analysis performed for the PTS issue, changes to the Fort Calhoun procedures should be made. These changes included the need to provide specific criteria for HPI and charging termination, and improved cautions to assure operator compliance with cooldown curves. These changes, and any other additional modifications based on the plant's analysis, were to be completed by June 1, 1982. These changes were included in the audit of the Fort Calhoun procedures and training. Fort Calhoun's procedures also require HPI operation for at leat 20 minutes, based on previous NRC requirements.

C.1.2.1.2 Present Operator Training

As described in OPPD's 60-day response, the operator training program is part of a two-phase effort by OPPD to address PTS. The first phase is performing analysis for PTS. The second phase is determining modifications, if any, that may be necessary, including procedure changes. Retraining of operators is to be conducted on the procedure revisions. Instruction on the PTS issue have been conducted for the operators and for all levels of OPPD's management.

C.1.2.1.3 Plant Audit

An onsite audit was conducted at Fort Calhoun to determine the level of operator understanding of present PTS concerns. This audit was conducted June 7, 1982. Preliminary findings from the audit indicate that the procedures were generally adequate for PTS, and are based on a plant-specific analysis performed by CE. The operators were generally knowledgeable of the PTS issue. Recommendations from the audit team included the upgrading of pressure-temperature curves, and the consolidation of NDT, saturation and subcooling curves onto one plot for more effective utilization of the curves by the operators.

C.1.2.2 Calvert Cliffs

C.1.2.2.1 Present Procedures

Development of Calvert Cliffs procedures is part of a two-phase program to address PTS. The first phase is the development of analyses for PTS. The second phase involves changing its plant operating procedures, if necessary. In BG&E's 60-day response to D. G. Eisenhut's August 21, 1981 request for information, specific procedural actions were provided, related to operation and termination of HPI and charging flow, and control of feedwater. An evaluation of the Calvert Cliffs procedures has been conducted by plant personnel, who feel that the procedures adequately address PTS, considering the risk involved. BG&E considers an integrated, analyzed approach to plant operations, of which PTS is one concern, to be the only reasonable approach to responsible plant operations. As stated in a letter dated May 4, 1982, from R. Bryant to D. Eisenhut, BG&E agrees with the staff that vessel integrity concerns should be properly addressed. Changes to Calvert Cliffs procedures have been made to remind the operators to observe the vessel integrity-related pressure temperature limits. BG&E stated that they will continue to be involved in the CE Owners' Group efforts for emergency operating procedures upgrades for NUREG-0737 Item I.C.1.

C.1.2.2.2 Present Operator Training

As described in BG&E's 60-day response, operator training will be conducted on any operating changes resulting from the plant's analysis. Operator training based on the changes identified was to have been completed by June 30, 1982.

C.1.2.2.3 Plant Audit

An onsite audit was conducted of the Calvert Cliffs procedures and training for PTS on July 6-8, 1982. The following changes to the Calvert Cliffs procedures were recommended: (1) provide clearer instructions for preferred methods of accident mitigation, (2) predetermine priorities of mitigative actions, and (3) upgrade procedure cross-references. Recommendations for training improvements included the need for additional training on accident mitigation methods, to include pressure-temperature control in various abnormal conditions (e.g., with and without vessel upper-head bubbles, and with and without forced circulation).

C.1.2.3 Maine Yankee

C.1.2.3.1 Present Procedures

Maine Yankee Atomic Power Company (MYAPC) provided a summary of their operator actions in their 60-day response to D. G. Eisenhut's August 21, 1981 letter to the eight plants being evaluated for PTS. These actions include criteria for HPI and charging termination and feedwater operation. The report further stated that a maximum subcooling limit was already in the plant's procedures. The subcooling limit is 200°F, and was based on pressurizer overstress concerns.

C.1.2.3.2 Present Operator Training

The training program described in the 60-day response included a discussion of operator training in emergency operating procedures, especially their training for maintaining 50°F subcooling. A more detailed training outline was provided in the 150-day response, and included technical as well as operational information. A schedule, as included in the 150-day response, showed that training for operating crews was to have been completed by June 1982, and that RO and SRO trainees would receive training in this area.

C.1.2.3.3 Plant Audit

A review of Maine Yankee's procedures and training for PTS was conducted on May 25-27, 1982. The review team found the plant operations personnel and STAs adequately knowledgeable of the PTS issue, and the procedures provided adequate guidance for preventing PTS. One significant operating philosophy already in place at Maine Yankee is the throttling of HPI flow to maintain as close to 50°F subcooling as possible during potential cooldown events. It was noted by the review team that no written exam was conducted after the lectures on PTS. Rather, a seminar method was used to determine the level of comprehension. Questions regarding PTS have been included in the written requalification examinations. The review team concluded that the operators were sufficiently knowledgeable of PTS. No changes to the operating procedures or training program were recommended to meet the objectives of the audit.

C.1.3 Babcock & Wilcox Plants

Oconee Unit 1 is the B&W-designed plant being evaluated for PTS. All analyses performed by B&W are specific to the Oconee plant.

C.1.3.1 Oconee

C.1.3.1.1 Present Procedures

Oconee 1's current emergency operating procedures cover design-basis events to cover the requirements of 10 CFR Part 50 Appendix B and were developed based on design analysis. In the Duke Power Company 60-day response to D. G. Eisenhut's August 21, 1981 letter to the eight licensees whose plants were being evaluated for PTS, a discussion of the procedural actions related to PTS were provided. The actions discussed included feedwater operation, HPI operation, and instrumentation. Also included was a graph of pressure vs. temperature, with allowable operating regions indicated for conditions with and without reactor coolant pumps running. In the graph's notes, the operators are instructed to maintain a 50°F to 100°F subcooling band with RCPs off. Duke Power Company stated in their 150-day response that based on the analysis presented in their letter, no major changes in existing plant procedures were considered necessary. The letter also stated that when implemented, the Abnormal Transient Operating Guidelines will include appropriate operator instructions for mitigation of overcooling transients.

C.1.3.1.2 Present Operator Training

As stated in Duke Power Company's 60-day response the Oconee operators receive instruction on HPI termination and feedwater control during requalification training. Training on plant response and emergency procedures is also conducted on the B&W simulator. The 150-day response further stated that Duke Power recognizes the importance of ensuring operators have sufficient training and the procedures are adequate to prevent the occurrence of severe thermal shock events. Additional training to augment operator understanding of PTS is to be conducted, but Duke considers the current knowledge of in-place plant procedures to be acceptable for the short term. Duke also stated that their operators have been made aware of the PTS concern, although no formal training has been conducted.

C.1.3.1.3 Plant Audit

A review of Oconee's procedures and training for PTS was conducted May 11-13, 1982. The site visit report is attached to this evaluation. In general, the review team found the operators adequately knowledgeable of the PTS issue, except that knowledge of past PTS events at other facilities was weak. The procedures provided mitigative actions to prevent PTS, but needed to be strengthened to provide actions if an unacceptable pressure-temperature condition was reached. The audit team felt that a means should be provided for plotting cooldown rate and subcooling margin with the plant computer out of operation.

C.2.0 Conclusions

Technical guidelines for Emergency Operating Procedures (EOPs) are being developed generically by the NSSS vendor owners' groups in response to TMI Action Plan Item I.C.1, "Short-Term Accident Analysis and Procedures Revision." These guidelines and their supporting analyses will address the actions required for mitigating a wide range of accidents and transients including multiple failures and operator errors. These guidelines will include the operator actions necessary to prevent or mitigate pressurized thermal shock. Incorporation of PTS concerns in the guidelines is beneficial and more effective than current procedures in that the analyses supporting the guidelines will verify that the mitigating actions for PTS do not result in inadequate core cooling or other problems. The Westinghouse Owners' Group has reviewed its existing Emergency Procedures Guidelines and is considering the PTS issue in developing the remainder of these guidelines. This effort was completed in July 1982. The B&W Owners' Group has incorporated desired operating regions in the Anticipated Transient Operating Guidelines (ATOG) for Oconee, which take PTS concerns into account. The B&W approach is considered acceptable until the long-term PTS program has been implemented. The CE Owners' Group has submitted draft Emergency Procedure Guidelines which provide a desired operating range for pressure and temperature. The CE guidelines are presently being reviewed by the staff. Another CE Owners' Group activitiy deals with verifying the "correctness" of the actions specified in the guidelines with respect to the

PTS issue. The revision of the guidelines to be submitted in August 1982 will include the results of this activity.

The NRC staff recognizes that the owners' groups' efforts on the emergency procedure guidelines would not be completed until Summer 1982 and staff review will not be completed until Fall 1982. The staff considers this schedule acceptable considering the low probability of occurrence of PTS events, the past operating history of PTS precursor events, and upgrades in instrumentation reliability resulting from the Rancho Seco and Crystal River events. Nevertheless, the staff has undertaken a program to audit the procedures and training covering pressurized thermal shock at the following plants: H. B. Robinson, Oconee 1, San Onofre 1, Maine Yankee, Fort Calhoun, Turkey Point 3, and Calvert Cliffs 1. The purpose of these audits is to assess the adequacy of current procedural steps and operator training necessary to mitigate PTS events, and to determine if corrective actions are required before the longer term PTS program provides acceptance criteria and generic resolution of the issue. The findings of the audits that have been completed at this time and the resulting plant-specific recommendations are discussed in this section. Audits of the remaining plants were completed by July 31, 1982. Additional plant-specific recommendations or generic recommendations may result from these audits.

Based on the audits conducted to date, the staff concludes that industry operators are generally knowledgeable of the PTS issue, and of the mitigative actions for PTS included in their procedures. Further, the procedures reviewed, with some specific exceptions delineated in the reports, provide a scheme for mitigation of PTS events. The procedures are usable for PTS, and can be understood by the operators.

C.2.1 Procedures

The seven plants currently being evaluated for PTS have reviewed their current emergency operating procedures for instructions relevant to the PTS issue.

H. B. Robinson's procedures, based on generic Westinghouse guidelines, included in its HPI termination criteria a minimum required pressure of 2000 psig, with HPI shut-off head at approximately 1500 psig. This could have resulted in extended HPI operation when not desired. Based on an NRC audit and the licensee's evaluation, H. B. Robinson has lowered the minimum pressure for HPI termination to 1560 psig, changed the temperature monitoring for operation from the RCS hot leg to the RCS cold leg, strengthened the emphasis on terminating HPI when its termination criteria are met, and provided more detailed instructions on RCS pressure and temperature control. The staff finds the H. B. Robinson procedural guidance adequate for the immediate PTS concern.

Turkey Point 3 procedures, based on generic Westinghouse guidelines, contain specific direction for HPI termination when the criteria are met. Personnel at Turkey Point 3 have reviewed the staff's H. B. Robinson audit report, and made changes to Turkey Point's procedures based on the findings from the staff's H. B. Robinson audit report. Based on these commitments the staff finds the Turkey Point Unit 3 procedural guidance adequate for the immediate PTS concern. Further verification was conducted during the onsite audit.

San Onofre Unit 1 procedures, based on generic Westinghouse guidelines and modified for the SEP evaluation, contain directive actions for termination of HPI and the information learned from the H. B. Robinson evaluation. The staff finds the San Onofre 1 procedural guidance adequate for the immediate PTS concern. Findings from the onsite audit are included in the audit report.

Fort Calhoun procedures provide some specific guidance to the operators for operation of HPI, charging, and feedwater. Omaha Public Power District (OPPD) has identified changes necessary to provide criteria for HPI termination to reflect the PTS concern, and improved precautions to assure operator compliance with cooldown-based pressure-temperature curves. The staff concurs with the need for these changes. A reevaluation of the requirement for running HPI for at least 20 minutes after initiation should be made by OPPD and the staff. We strongly recommend removing any minimum running time requirements for HPI. A more detailed evaluation of Fort Calhoun procedures was conducted durng the onsite audit.

Calvert Cliffs procedures provide some specific guidance to the operators for operation of HPI and feedwater. Cautions and a Technical Specification are intended to provide assurance that HPI or feedwater will be terminated prior to vessel challenge. The instructions, by themselves, do not provide the specific guidance the staff feels is necessary. They should include a directive action step for control of HPI. A determination was made during the plant audit that some procedures modifications are necessary for the operators to effectively deal with PTS.

Maine Yankee's procedures provide some specific guidance to the operators for operation of HPI, charging and feedwater, including a subcooling band (50°F minimum, 200°F maximum). Maine Yankee has requested, in their discussions, that the staff reevaluate its position on requiring HPI flow for a minimum of 20 minutes, and on requiring immediate RCP trip after a safety injection. We concur that this needs to be done before PTS can be completely addressed in any plant's procedure. The staff finds the Maine Yankee procedural guidance adequate for the immediate PTS concern.

Oconee Unit 1's procedures provide some specific guidance to the operators for operation of HPI and feedwater. When below 500°F, the operators are instructed to maintain a subcooling band (50°F minimum, 100°F maximum). The operator is specifically directed to throttle HPI when 50°F subcooling is reached. The staff finds the Oconee procedural guidance adequate for the immediate PTS concern.

C.2.2 Training

The seven plants currently being evaluated for PTS have all stated that they are augmenting their operator training for PTS. The staff conclusions regarding individual plants are included in each audit report.

C.2.2.1 Improvements in Emergency Operating Procedures

Westinghouse performed an evaluation of procedural actions for PTS by reviewing, step by step, guidelines that have a realistic technical basis. In reviewing the technical basis for each step, a determination could be made of its applicability to the PTS concern. This program shows the importance and viability of an integrated approach to accident mitigation, where new technical problems can be evaluated in a manner that includes incorporation of concerns of other technical issues. Combustion Engineering and Babcock & Wilcox have done a significant amount of work on developing their own approach to generic guidelines. All three owners' groups are developing guidelines to be function-oriented, in accordance with NUREG-0737, Item I.C.1. This approach to accident mitigation will provide a means to significantly reduce operator error by providing mitigative actions that are not dependent on diagnosis of specific transients or accidents. This approach will increase the accuracy of operator response by reducing complex diagnostic problems to a prioritized, simplified, function-level response that will be used even if an event is incorrectly diagnosed.

The staff concurs, and strongly encourages, the approach stated by the seven plants being evaluated to ensure that the guidelines and subsequent plant procedures developed in accordance with NUREG-0737 Item I.C.1 address PTS, as well as coordinate the PTS actions with actions to mitigate other serious transients or accidents. We believe this is the best method to provide an integrated set of emergency operating procedures to deal with a wide range of transients and accidents, and will provide the analytic base for evaluation of future technical problems.

In reviewing industry responses to comply with NUREG-0737 Item I.C.1, the staff will review the technical guidelines for emergency operating procedures (EOPs) and will review a description of how EOPs are developed from the guidelines for each operating plant. This will provide assurance that procedures at each plant will be based on analysis of PTS and other events. This review will be performed for all operating reactors and operating license applicants.

C.3.0 <u>Recommendations</u>

(1) The staff should complete the audits of the remaining plants currently being evaluated for PTS, using the following review criteria:

   (a) Procedures should not instruct operators to take actions that would violate NDT limits.

(b) Procedures should provide guidance on recovering from transient or accident conditions without violating NDT or saturation limits.

(c) Procedures should provide guidance on recovering from PTS conditions.

(d) PTS procedural guidance should have a supporting technical basis.

(e) High pressure injection and charging system operating instructions should reflect consideration for PTS.

(f) Feedwater and/or auxiliary feedwater operating instructions should reflect PTS concerns.

(g) An NDT curve and saturation curve should be provided in the control room. (Appendix G limits for cooldowns not exceeding 100°F/hr.).

(h) Training should include specific instruction on NDT vessel limits for NORMAL modes of operation.

(i) Training should include specific instruction on NDT vessel limits for transients and accidents.

(j) Training should emphasize those events known to require operator response to mitigate PTS.

(k) Training should include simulator operation in responding to PTS transients including recovery from PTS conditions, and control room walk-throughs of PTS events.

These audits were conducted in July 1982. Reports on the results of the audits are available separately.

(2) If any other plants are determined to be of immediate concern for PTS, the staff recommends requiring those licensees to conduct an audit of their own procedures and training for PTS, using more specific criteria

developed from that stated in item 1, which will include a method for ensuring a balance of technical concerns.

The persons conducting the audit should collectively have expertise in plant operations, systems, training, procedures development, and fracture mechanics.

This item should be completed promptly as plants of immediate concern are identified.

(3) The NRC staff should ensure that actions to mitigate PTS are included in the guidelines being developed for NUREG-0737 Item I.C.1. (See Section 8.2.4 for a discussion of the guidelines' status.) Included in their review of the analyses upon which the guid lines are based, the NRC staff should ensure that PTS concerns have been adequately analyzed, and a balance of considerations is included in the actions specified in the guidelines.

This item should be completed (including staff review) within 2 years.

(4) Licensees should verify that guidelines discussed in item 3 address the following concerns:

(a) Instructions should include allowance for system response delay times (e.g., loop transport time, thermal transport time).

(b) The need for cooldown rate limits for periods shorter than one hour should be evaluated.

(c) Methods for controlling cooldown rates should be provided.

(d) Guidance should be provided for the operator if cooldown rates or brittle fracture limits are exceeded.

(e) The desired region of operation (e.g., subcooling band) on the pressure-temperature curve should be evaluated to determine if it can be revised to maximize the operator's ability to prevent brittle fracture.

(f) Instructions for controlling pressure following depressurization transients should be provided.

This item should be completed in the same time frame stated in item 3.

(5) The staff recommends that the initial training on the procedures developed from the guidelines discussed in recommendation 3 above include a specific section on the technical concerns of PTS, and the specific manner in which the procedures provide the mitigative actions. This training should be integrated into each plant's overall training program.

(6) The staff recommends that training programs for periodic operator requalification include the recommendations of item 5 above.

This item should be implemented at the first requalification training cycle following implementation of the upgraded procedures.

(7) Additional recommendations may result from the audits conducted for 1 above.

The staff feels that these recommendations are the most balanced approach to ensure the adequacy of operator response to PTS events. This is accomplished by determining the adequacy of operator understanding at the plants of most concern, then providing for all plants the best available means to ensure the procedures used for plant operation cover a wide range of transients and accidents, while covering a wide variety of multiple failures.

APPENDIX D

REACTOR VESSEL FRACTURE MECHANICS ANALYSIS

The vessel integrity analyses, the results of which are reported in this document, include a determination of the temperature distribution across the vessel wall versus time, the thermal stresses as a consequence of this temperature distribution, as well as fracture mechanics results. The analyses were performed either by the NRC staff using its in-house program or by ORNL using the OCA program. These programs are described in the following sections. Illustrations of typical temperature, stress and stress intensity factor distributions across the vessel wall at a certain time in the transient are shown in Figures D-1 (a), (b) and (c) respectively. It should be noted in Figure D-1 (c) that the stress intensity factor, K_I, for long axial cracks is higher than for long circumferential cracks, especially for cracks that extend relatively deep into the vessel wall. K_I is due to contributions from thermal stresses, pressure stress and other stresses that may be present. Superposed in Figure D-1 (c) are K_{Ic}, the vessel toughness that determines crack initiation, and K_{Ia}, the toughness at crack arrest. When K_I exceeds K_{Ic}, crack initiation is expected (for axial cracks having depths between points C and C^1 in the diagram), if warm prestressing is not effective (warm prestressing is discussed in D.3). The crack would then grow to a depth where K_I intercepts the arrest curve, K_{Ia} (point A in the diagram). Similar results would occur for a circumferentially orientated crack except that arrest will generally occur at the shallower depths.

Equivalent calculations are made at other times into the transient and the results cross-plotted on a critical crack depth diagram as shown on Figure D-2 (b). Also shown in Figure D-2 (b) is the depth at which the upper shelf toughness of the metal is reached (nominally 200 ksi $\sqrt{in}$). If the arrest point falls above the upper shelf, arrest is assumed not to occur.

Figure D-2 (a) illustrates the trend of K_I for a particular crack depth versus time for a hypothetical PTS transient. If pressure remains constant or decreases with time, K_I will increase to a maximum and then decrease as the thermal stresses die out. The time at which K_I reaches its maximum determines the time of warm prestressing (WPS). When the entire initiation curve falls to the right of the WPS time as shwon in Figure D-2 (b), crack initiation is not expected to occur; however, care must be exercised in reaching this judgment because of analytical and material uncertainties. The dotted line in Figure D-2 (b) indicates that crack initiation might occur because of uncertainties and might reinitiate later in time because of an increase in K_I at that time. If this were to occur, arrest is not expected because then the arrest curve is above the upper shelf toughness.

D.1 NRC Analytical Procedures

The NRC procedure to evaluate the effect of cooldown transients and postulated accident scenarios on the integrity of reactor vessels was developed in 1978 and subsequently updated to include technological data as it becomes available. It is designed primarily for investigations of thermal shock to the beltline region of vessels with a vessel radius to wall thickness ratio of about ten.

Heat transfer algorithms are based on classical closed form solutions which provide temperature distributions across a vessel wall versus time into a cooldown transient. These temperature profiles are used to calculate thermal stresses versus time and depth into the wall. The calculation of fracture mechanics stress intensity factors is based on the linear-elastic boundary integral equations method for cylinders and the superposition of stresses due to all causes particularly those due to temperature differences, pressure and residual stresses in welds. Although certain simplifying assumptions are used in the procedure, its results have been compared with those from more sophisticated analyses and found to be in good agreement.

D.1.1. Assumptions

Geometry

For heat transfer and thermal stress analyses, slab geometry is assumed. For typical reactor vessels with a vessel radius to wall thickness ratio of ten or more, the error introduced by this assumption is negligible compared to other uncertainties inherent in the analyses. This assumption permits a more simple calculational procedure that is adaptable to programmable calculators or computers. Cylindrical geometry is used, however, in the fracture mechanics analyses.

Heat Flow

In a cooldown or heatup transient, heat flow is assumed to occur only in the wall thickness direction. Thus, the procedures are one-dimensional.

Heat Transfer Coefficient

The heat transfer coefficient, h, during a typical transient can vary over a considerable range depending on the hydraulic and thermal conditions. Its magnitude may even be difficult to determine versus time as the transient progresses because of hydraulic and other uncertainties. However, for values of heat transfer coefficients in the range of interest for most thermal transients (approximately 300 Btu/hr ft^2 °F), short perturbations to higher values do not cause significant increases in thermal stresses. Therefore, for typical transients of interest, metal temperature and stress distributions are obtained by utilizing a constant heat transfer coefficient. The value used is conservatively selected on the basis of experience and judgment. For maximum conservatism, a value of infinity can be used. The heat transfer coefficient is also assumed to be the same at all water cooled portions of the vessel wall.

Temperature Dependence of Metal Properties

The physical properties of materials are temperature dependent. When thermal transients result in a significant temperature range and difference through the vessel wall, accurate results require consideration of this phenomenon. Data for materials of interest are taken from recent ASME publications.

Analytical Model

Prior to the thermal transient, the water temperature is assumed to have remained constant for a sufficiently long time so that the vessel wall temperature is at a uniform temperature equal to the water temperature. Prior to and during the transient, heat flow at the outer insulated surface of the vessel is assumed to be zero and the vessel cooled or heated only at the inner surface with no sources of heat within the metal. For typical transients of interest, these assumptions introduce minimum uncertainties in the end results.

Finite Number of Series Terms

Solutions for metal temperature distributions at various times during a transient are in the form of an infinite series. Because of obvious practical considerations, it is necessary to truncate the series to a finite number of terms. The error introduced by limiting the number of series terms is significant only at or very shortly after the start of the transient (time = zero) where an infinite number of terms is required to obtain correct temperatures. Shortly thereafter, however, higher terms in the series decay rapidly to insignificant values. Because, for transients of interest, the maximum thermal stresses generally occur relatively late in the transient, little or no error is introduced by utilizing a finite number of terms. Six series terms are used for deterministic analyses; however, the last two terms contribute very little. Therefore, for probabilistic analyses only four terms are used.

Effect of Cladding

Because the material and physical properties of the stainless steel cladding differ from those of the carbon steel wall, the cladding effect must be accounted for in reactor vessel integrity analyses. The presence of cladding affects the heat transfer and stress calculations as well as the fracture mechanics analyses. The heat transfer coefficient is readily adjusted to account for the higher thermal resistance of the stainless steel clad (Figure D-3). The stress effect of the clad, however, depends on the stress relief and operational history of the vessel. Once this is established, this effect is accommodated by superposition of cladding induced stresses with those from other causes including those due to temperature variations across the wall of the vessel (Figure D-4). Fracture mechanics effects of cladding depend on the assumed shape and location of postulated cracks. Procedures for treating long through-clad cracks are used. The treatment of elliptical cracks needs further development. In general, thermal stresses and stress intensity factors for long through-clad cracks are calculated assuming only the thermal resistance of the clad, calculating the stresses and stress intensity factors assuming a constant metal temperature and superposing the results. Thus, the effect of cladding is accounted for in the heat transfer, thermal stress and fracture mechanics analyses when long through-clad cracks are assumed.

The NRC model for determining the clad effect for postulated long through-clad cracks is as follows:

- Assume that the clad is stress-free at reactor operating temperature. As the vessel wall cools down, tensile stresses in the cladding and lesser compressive stresses in the base metal develop and reach a clad stress of about 30 ksi at room temperature.

- The average clad temperature is assumed to be the cooled surface temperature during a transient; however, to determine the incremental effect due to the clad, the entire wall temperature is assumed to be constant (the effect of the actual temperature variation across the wall during a transient is superposed later). The lower thermal conductivity of the cladding is included in the determination of the surface temperature by a reduction in the heat transfer coefficient.

- Knowing the clad incremental stress, the stress intensity incremental effect due to the clad is then calculated via the influence function technique described briefly in Section D.1.4.

The results of an example calculation of the clad effect on the stress intensity factor as determined independently by the staff and ORNL are shown in Figure D-5.

D.1.2 Stress Algorithms

The total peak stresses (thermal plus pressure plus residual plus any other stresses) are assumed to be less than, or at least not significantly larger than, the material yield strength so that components of stress can be added and that linear-elastic fracture mechanics procedures can be utilized. For rapid thermal transients, high stresses usually occur locally at the inner vessel wall and acceptable stress distributions (total stress below yield) over the remaining section can still be obtained if the overstressed region is relatively thin.

D.1.3 Postulated Initial Cracks

Long through-clad cracks, either axial or circumferential, are assumed to exist in the welds of limiting (highest) RT_{NDT}. In this case, the cladding effect is conservatively applied in that the stresses due to the different expansion coefficients of the clad and base metal are added to the nominal thermal stresses. For short through-clad cracks or underclad cracks it is conceivable that the cladding can have a beneficial effect if the cladding is sufficiently tough, that is, it is less affected by irradiation damage than the base material. In this case, it could deter crack elongation or could even prevent crack initiation depending on the specific transient. At present, there are differences of opinion as to clad toughness after irradiation, and further research is needed as to the behavior of short or underclad cracks in an overcooling event. Also, analyses to date omit consideration of weld residual stresses and in the case of circumferential cracks, the effect of dead weight stresses. Therefore, the NRC concludes that the more conservative assumption of long through-clad cracks should be used at least for scoping calculations, until further information is developed to permit a relaxation of this assumption.

D.1.4 Fracture Mechanics Algorithms

Fracture mechanics analyses utilize the linear-elastic boundary integral methods of Heliot, Labbens and Pellissier-Tanon (References D.1 and D.2).

At each time step, the thermal and other stresses are expressed as polynomial functions of the relative depth into the wall of the vessel:

$$\sigma \left(\frac{X}{L}, t\right) = \sum_{j=o}^{4} \sigma_j \left(\frac{X}{L}\right)^j$$

where σ_j's are constants determined by curve fitting. The stress intensity factor for this stress distribution is then;

$$K_I = \sqrt{\pi a} \sum_{j=o}^{4} \sigma_j \left(\frac{a}{L}\right)^j i_j$$

In the NRC procedure, the i_j's are expressed as polynomial functions of the relative crack depth. Different expressions for the i_j's are used for different crack geometries and directions.

The stress distribution due just to the cladding, however, cannot be expressed by a polynomial equation without resorting to a large number of terms. For this application, the staff used the basic equations in the references and adapted them to obtain an expression for long axial cracks in a cylinder (expressions for other crack geometries and directions need further development):

$$K_I = \sqrt{\pi a} \left(\frac{2}{\pi - 2}\right) \left\{ \left(i_o - \frac{2}{\pi}\right) \int_o^{\frac{\pi}{2}} \sigma(\omega)\, d\omega - (i_o - 1) \int_o^{\frac{\pi}{2}} \sin \omega\, \sigma(\omega) d\omega \right\}$$

where:

$$\sin \omega = \frac{X}{a}, \quad o \leq \chi \leq a$$

and i_o is the influence function for a uniform stress.

D.2 ORNL Analytical Procedures, OCA-I, OCA-II

In addition to performing its own PTS analyses, the NRC staff also utilized the services of ORNL. The ORNL analytical code differs from that of the NRC, yet compatible results are obtained. The ORNL program is described in Reference D.3 from which some of the following is taken.

The OCA-I code is a computer program that performs a two-dimensional linear-elastic fracture mechanics analysis for long axial inner-surface flaws in a cylinder subjected to time-dependent thermal and pressure loadings. Six basic calculations are performed: (1) a one-dimensional thermal analysis to obtain temperature distributions through the wall of the cylinder as a function of time; (2) stress analysis, neglecting presence of flaw, using thermal and pressure loadings; (3) calculation of stress instensity factor (K_I) as a function of flaw depth and time; (4) calculation of static initiation and arrest toughness values (K_{Ic} and K_{Ia}) at tip of flaw as a function of flaw depth and time; (5) calculation of K_I/K_{Ic} and K_I/K_{Ia} as a function of flaw depth and time; and (6) construction of the critical-crack-depth curves, which indicate the behavior of the flaw at all times during the transient.

Input to the thermal analysis includes the coolant temperature vs. time, the fluid-film heat transfer coefficient, and the initial temperature of the cylinder. All necessary material properties, with the exception of the reference temperature (RT_{NDT°) and the concentrations of specific impurities (copper and phosphorous), are included in OCA-I, but different values may be inputted. The calculation of K_{Ic} and K_{Ia} considers the temperature and fast-neutron-fluence distributions through the wall, RT_{NDT° and the copper and phosphorous concentrations, which influence the radiation damage effect.

The K_I calculation is based on a superposition technique that uses the uncracked-cylinder stresses and a set of unit-load K_I values (K^*) that correspond to cylinder dimensions typical of a 1000-MW(e) pressurized-water reactor pressure vessel (4.37-m ID x 4.80-m OD). The K^* values were calculated using finite-element techniques and are included in OCA-I.

The development of OCA-I was prompted by a growing interest in the behavior of surface flaws in reactor pressure vessels during overcooling accidents. The OCA-I code was designed specifically for these accidents in an effort to minimize time and expense associated with the analysis. To this end, special provisions were made for parametric-type analyses. OCA-II, which was used for later studies, includes plotting refinements plus the incorporation of the cladding effect in the stress intensity factor.

The OCA-II code (Reference D.4) which was developed by Oak Ridge National Laboratory utilizes:

(a) the latest NRC calculation method for determining neutron fluence attenuation with depth into the vessel wall, which is described in Section D.4 of this report,

(b) the latest NRC calculation method for determining shift in RT_{NDT} with neutron fluence, which is described in Section D.4 of this report,

(c) a finite element, one-dimensional code with a constant heat transfer coefficient, h, in thermal analyses,

(d) thermal, pressure and clad stresses and infinitely long axial through-clad crack in the finite element linear-elastic fracture mechanics (LEFM) analyses,

(e) any prescribed water temperature during the transient.

The OCA-II code and NRC LEFM analyses performed for this study do not include plate-to-plate weld residual tensile stresses. We believe that OCA-II and NRC stress, thermal and fracture mechanics analyses are sufficiently conservative to permit a parametric study of vessel fracture without including these stresses.

D.3 Warm Prestressing

Although warm prestressing (WPS) can theoretically prevent crack initiation during a pressurized thermal shock transient, the staff believes that the fluctuations of pressure and temperature during these transients are possible; therefore, our scoping calculations did not rely upon WPS to prevent crack initiation. The NRC staff believes that it would not be prudent for operators to rely primarily on warm prestressing to assure reactor vessel integrity during pressurized thermal shock transients. The staff is aware of and accepts the theoretical basis for warm prestressing. One explanation for the WPS effect is:

"During temperature reduction, initiation of crack propagation from an arrested crack in the reactor vessel cannot occur while the K value is constant or decreasing." (Reference D.5)

Another explanation rests on a physical picture of blunting at the crack tip and development of favorable residual stresses caused by the warm prestress.

The theoretical basis for warm prestressing assumed that K_I is decreasing with time in <u>monotonic</u> fashion after it reaches its maximum value. In a real transient, the pressure component of K_I may rise and fall in an unpredictable fashion as the system is being brought to a stable condition. Some variation in the thermal component of K_I may also occur. Of particular concern to the staff is that emergency operating procedures at some facilities permit repressurization after a thermal transient to as high as 2000 psig. Thus, the potential benefit effects of WPS may be deliberately defeated.

Experiments have shown that when there is an increase in K_I after cooldown to a temperature at which K_I exceeds K_{Ic}, there is an ever-increasing probability of fracture as K_I increases such that the probability is very nearly one for $K_I = K_I$ maximum. The probability of fracture decreases to acceptably low values for $K_I - K_{Ic} \cong 25$ percent of (K_I max. - K_{Ic}). (Reference D.6) The experimental information also shows clearly that the beneficial effects of warm prestress are nearly eliminated if K_I drops to a low value after reaching K_I-maximum and then increases, for example, repressurization late in transient after the vessel has cooled down.

During a typical transient scenario, the reactor coolant temperature and pressure both decrease initially from their normal operating values. Thereafter, the trend of both temperature and pressure depends on the nature of the event and the actions taken by operators and/or automatic systems. Because of the relatively rapid decrease of the reactor coolant temperature, thermal stresses are developed in the vessel wall which are superposed on the pressure stresses.

The net result is an increase of the total stress intensity factor, K_I versus time. The thermal component of K_I reaches a maximum and then decreases and the wall temperature tends toward a uniform value. Typically, the total K_I also has a maximum during this initial period. Thereafter, the change in K_I versus time depends on the assumed actions taken by operators and by automatic systems.

There are, of course, many possible variations in the cooldown scenario that will produce different degrees of departure from the ideal monotonic decrease of K_I after reaching K_I-maximum. Our knowledge is insufficient to draw the line between acceptable versus unacceptable transients with regard to the acceptance of warm prestressing, other than to say that we ought not to rely on it at this time. The exceptions are transients such as certain LOCAs where pressure is limited as described below.

Following a severe cooldown transient, the NRC staff believes that facility operators should limit reactor pressure by manual and/or automatic means to the extent practicable. Preferably, reactor pressure should be decreased monotonically consistent with 50°F subcooling and the pressurizer water level increased only to its normal operating range. In particular, water-solid conditions should be avoided especially if the reactor coolant reaches low temperatures. Repressurization should not be permitted until the transient has been evaluated, and for severe transients, the vessel should be inspected to assure its integrity.

Even if these procedures are followed, it still is conceivable that a small crack may initiate and grow deeper. However, in the absence of pressure, it will not penetrate the wall. With pressure stresses also present, it is possible that a crack would create an opening in the vessel, especially when the wall material has cooled down.

In conclusion, the staff believes that it would not be prudent to rely on warm prestressing to assure reactor vessel integrity during a pressurized thermal shock transient. The basis for this position rests on uncertainties regarding system considerations and on insufficient experimental information to confirm the benefits of warm prestressing under these circumstances at this time.

While the odds in favor of warm prestressing being a viable phenomenon to prevent initiation or reinitiation of a crack during a particular transient scenario may be relatively high, facility operators should also consider the relative risk.

For small break LOCAs of sufficient size such that the pressure is limited to some low value during the critical period of the transient or is monotonically decreasing because of the inability of the ECC and charging systems to maintain high values, then conditions are attained where warm prestressing can be effective and credit can be considered for it.

D.4 Determination and Utilization of Material Toughness

To make the fracture analyses of pressurized thermal shock, it is necessary to have values for the fracture toughness of the material at the tip of the postulated cracks in the reactor vessel wall. Toughness must be known as a function of time in the transient, and temperature and fluence must be known as a function of position in the wall.

D.4.1 ASME Code Section XI Curves

The fracture analyses performed by utilities, vendors and the NRC have all utilized the values of K_{Ic} and K_{Ia} given in Section XI of the ASME Code and reproduced in Figure D-6. The toughness values are given as a function of the temperature, T minus RT_{NDT}, the reference temperature, nil-ductility transition. The quantity, RT_{NDT} is the sum of two quantities; the initial RT_{NDT} and the ΔRT_{NDT} caused by irradiation. Appendix E of this report describes the bases for estimating initial RT_{NDT} and ΔRT_{NDT} for the individual plants. Estimates are given for the inside surface of the vessel wall (at the clad-base metal interface) for the critical locations, which are almost always the welds, either a longitudinal weld or a circumferential weld in the beltline. The second step is to determine the attenuation of ΔRT_{NDT} through the vessel wall.

D.4.2 Attenuation of Fluence and RT_{NDT} through the Vessel Wall

Some recent changes have been made in the way the attenuation calculations are made. These are illustrated in Figure D-7. In the past, the attenuation of fluence has been calculated by an exponential equation fitted to the results of calculations given in surveillance reports, as follows.

$f = f_o e^{-.33x}$

f = fluence at any point, n/cm² ($E \geq 1$ MeV)

f_o = fluence at inside wall

x = distance from inside wall, inches

However, changes in the neutron energy spectrum within the wall cause the use of the above formula to be unconservative. Therefore, the NRC has chosen to use displacements per atom (dpa) as the damage function, following a report received from HEDL (Reference D.7). They provided six plots of the ratio, dpa/fluence ($E \geq 1$ MeV), versus depth in the vessel wall. At 8.0 inches, the ratio averaged 2.06. To achieve this reduction in the attenuation at 8.0 inches, the equation for fluence attenuation becomes:

$$f = f_o e^{-.24x}$$

Thus, we use a "dpa equivalent" attenuation equation, while retaining the description of fluence in terms of n/cm² ($E \geq 1$ MeV).

As illustrated in the lower part of Figure D.7, the combination of the dpa-equivalent equation for attenuation of fluence and the Guthrie trend curve formula gives an expression for the attenuation of RT_{NDT} that is much less steep than that previously used. We believe that the new expression is realistic and have incorporated it into the OCA-II code described in Section D.2.

D.5 Stress/Fracture Mechanics Procedures Summary

The analytical methods used by NRC, ORNL and vendors differ somewhat but yield essentially the same results if all input assumptions are the same. Differing conclusions result primarily from assumptions as to crack shape, clad effects, effect of warm prestressing, etc.

When materials properties and the transient are known, these procedures can predict crack behavior quite well as demonstrated by results from the ORNL thermal shock experiments.

For generic studies, the NRC uses an exponential decay of water temperature to envelope a variety of transients. The staff has also used the Rancho Seco event as an analytical model. Our objectives are:

Avoid crack initiation, if possible.
Avoid vessel failure, in any event.

The staff has studied the PTS issue both deterministically (conservative assumption) and probabilistically (mean values of parameters) to assess risk to a vessel.

D.6 Discussion of Results

The NRC has performed both deterministic and probabilistic fracture mechanics analyses to generate a basis for judgment regarding the safety margins against PTS transients especially for the more highly irradiated vessels. Although recognizing that the transients that occurred at Rancho Seco in 1978 and Ginna in 1982 are unique, and are very unlikely to happen in the same way again, the staff concludes that they provide measures of the severity of a PTS event. The NRC and ORNL have arbitrarily utilized an idealized Rancho Seco pressure-temperatre transient as a benchmark model for other vessels.

For generic investigations, however, a postulated exponential decay of water temperature has proved to be more appropriate in that it can be characterized by two parameters, β (min.$^{-1}$) which is the reciprocal time constant and T_f (°F) the final postulated equilibrium temperature. The initial temperature, T_o, is the normal operating temperature. Thus, $T_w = T_f + (T_o - T_f)\, e^{-\beta t}$ Information obtained from transients that have actually occurred at nuclear facilities indicates that the above formulation adequately describes the water temperature at least for the initial critical portion of the transient. After T_f is approached, operator and/or systems action can, of course, affect the longer term variation of water temperature. Typical values of β have been found to be in the range of 0.05 to 0.15 min.$^{-1}$ as a consequence of the physical limitations of a real facility. Higher values of β of about 0.5 min.$^{-1}$ or more have been estimated for hypothetical, low probability design basis transients. Typical values of T_f for the worst cooldown transients to date (including those at BWR facilities) are in the range of 250 to 300°F. The Rancho Seco event, for instance, resulted in β of about 0.05 min.$^{-1}$ and a T_f of about 290°.

For the more likely transients, the times of crack initiation have been calculated to be 20 to 30 minutes or more after the onset of cooldown, the actual time varying up to one hour depending on the pressure and RT_{NDT}. Thus, operators have time to gain control of the event if properly instructed and trained.

The OCA II Code was utilized to determine the lowest RT_{NDT} for crack initiation as a function of constant pressure, final water temperature (T_f) and the reciprocal time constant (β). From these data, Figures D-8 and D-9 were plotted which indicate the effect of T_f, β and pressure on crack initiation.

The principal objective of the NRC (and the industry) is to prevent crack initiation, and for more probable PTS events, this may be possible. However, for the less likely events such as a postulated small break LOCA, crack initiation is likely in vessels with a relatively high RT_{NDT}. For these cases, the objectives must be to prevent any crack from propagating through the wall. Early in the transient, a pre-existing crack can initiate and propagate to the order of half the wall thickness or somewhat less, and then arrest because the metal at this location is still much warmer than at the cooled surface and because the metal at this depth has experienced less irradiation damage. As previously mentioned, linear elastic fracture mechanics (LEFM) methods are used for analysis of PTS transients. Typical values of K_I at the first crack initiation range from 60 to 100 ksi $\sqrt{in}$.

LEFM is not valid for tough materials such as are encountered on or above the upper shelf. Other techniques are necessary. These techniques have been developed for analysis of piping flaws and for pressure vessels under pressure loads only (Task Action Plan A-11). To date, they have not been adequately developed for treatment of more complex stress patterns such as occurred in a PTS event.

Therefore, the NRC conservatively assumes that, if a crack is calculated to propagate above the upper shelf of the material (200 ksi $\sqrt{in}$ is assumed), it is assumed to continue propagating through the wall. It is recognized that subsequent elastic-plastic or fully plastic analyses may show that this may not be the case. On the other hand, it must be recognized that if the pressure is high enough, crack propagation through the wall is possible, even in tough material, because the remaining ligament may not be sufficient to sustain the pressure and residual thermal stress loads. Pending further research in this area, the NRC concludes that a conservative approach must be taken.

D.6.1 OCA-II Parametric Study

The OCA-II code was used to make a parametric study of the effects of pressure P, final water temperature, T_f, and the reciprocal time constant, β, on the critical values of RT_{NDT} at the inside wall for crack initiation and crack penetration through the wall (no arrest). (Strictly speaking, initial RT_{NDT} should be mentioned as a variable, because it is only ΔRT_{NDT} that attenuates through the vessell wall, but the difference in critical values of RT_{NDT} for different initial RT_{NDT} values is negligible.)

Some of the results of the parametric study, plotted in Figure D-8, show that the T_f - RT_{NDT} is a fairly rasonable normalizing parameter, although the curves for different T_f values are separated by as much as 10-20 degrees at low pressure. Figure D-8 indicates that crack initiation will occur at lower material RT_{NDT} as pressure increases or final water temperature decreases. Figure D-9 indicates that crack initiation will occur at lower material RT_{NDT} as β increases, but, the effect is slight for values of β greater than 0.15. The "dogleg" in the curves of Figures D-8 and D-9 occurs because the critical crack size changes. At low pressure, K_I - thermal predominates in the fracture analysis and the critical crack sizes are a fraction of an inch, whereas at high pressure the critical size is near the arbitrary limit of 1.25 inches.

A cross plot of these Figures, shown in Figure D-10, illustrates the effect of pressure and T_f on the critical value of RT_{NDT}, for a given value of β (0.15 min.$^{-1}$). To use Figure D-9, the plant condition as characterized by RT_{NDT} is related to the transient severity as characterized by P, T_f and β to determine if the vessel is safe from crack initiation. This is, of course, a deterministic calculation, which assumes that the critical flaw depth given by the analysis is indeed present in the critical weld. Stated in another way, if the value of RT_{NDT} used is the true value, the probability of crack initiation is the probability that the critical flaw is indeed present.

Also shown in Figure D-10 is a set of "no arrest" lines, which merge with the solid lines for crack initiation at about 600 psig. This means that at very low pressure, cracks will arrest if T_f is between the solid line and the dashed line. At higher than 600 psig, the analysis shows that a crack, once it has initiated, will penetrate the vessel wall. The assumptions on which this analysis is based are thought to be conservative--they assume that the material will behave as indicated by linear-elastic fracture mechanics.

Finally, in Figure D-10 there is a steeply slanting dashed line marked "Circumferential cracks." It was drawn on the basis that at low pressure K_I - thermal is the same for cracks of any orientation (which is nearly true for shallow cracks) and on the basis that K_I - pressure for circumferential cracks is approximately one-half of that for axial cracks.

The fluid film heat transfer coefficient, "h", is another variable (in addition to T_f, P and β) that is part of the characterization of a transient. The parametric study described above was made using an "h" of 1000 Btu/hr ft^2 °F, which is characteristic of a "pumps on" condition. To check the effect of a change in "h" to 300, for a "pumps off" condition, eight cases were repeated, using OCA-II. The results, shown in the following table, are the differences in critical RT_{NDT} (in degrees F) for a calculation using h = 300 minus the result for h = 1000. As expected, a higher value of RT_{NDT} can be tolerated when "h" is lower, but the difference is only about 10°F or less at high pressures. The difference is seen to be greatest at low pressure, where K_I - thermal is the predominant part of K_I - total, and for a severe cooldown. This means that the near vertical lines of Figure D-10 would move to the left 5°F at P = 2500 psig and about 29°F at 500 psig. Figure D-11 is a repeat of Figure D-10 for h = 300 instead of 1000.

	T_f = 150°F		T_f = 300°F	
	β = 0.015	β = 0.15	β = 0.015	β = 0.15
P = 500 psig	9	29	0*	25
P = 2500 psig	11	5	7	5

*Both calculations stopped at RT_{NDT} = 400°F

D.6.2 Fracture Mechanics Analysis for Several PWR Recorded Transients

In the past, a number of events have occurred that can be categorized as PTS transients. Some of these have previously been analyzed by fitting the actual temporal temperature and pressure variations with smoothed and/or bounding curves in order to facilitate the analysis. These transients have recently been reanalyzed using the recorded temperature and pressure traces with all their respective fluctuations. The results are presented in an ORNL report, Appendix O, and as is discussed elsewhere in this document, were used as part of the basis in arriving at RT_{NDT} screening criteria.

D.6.3 Fracture Mechanics Example Analyses

In addition to the many uncertainties regarding PTS scenarios such as the temperature and pressure profiles versus time, the degree of mixing of cold water with warm water, etc. there exists parametric uncertainties in the stress and fracture mechanics analyses. The treatment of these uncertainties becomes significant when the cooldown temperature is to approximately RT_{NDT} because small changes in assumptions can influence whether or not a crack will initiate.

Assuming infinitely long cracks, h = 330 Btu/hr ft^2 °F (including clad effect) and using, for example, an assumed downcomer water temperature transient of

$$T_w = 250 + 300\ e^{-0.15t}\ °F, \quad t = \text{minutes}$$

which is only slightly more conservative than transients that have actually occurred and RT_{NDT} at the cooled surface of 294°F which is only slightly greater than that which exists in some facilities, the NRC staff found that to prevent crack initiation, the pressure versus time would have to be less than as shown in Figure D-12. That is, the pressure should be reduced to near saturation conditions by about 30 minutes if warm prestressing (WPS) is assumed to be ineffective. If the pressure had been reduced approximately monotonically, then WPS, which occurs at about 18 minutes for this assumed transient, could also preclude crack initiation. From the results of this transient provided by ORNL, which were calculated using somewhat more conservative assumptions regarding input parameters, crack initiation was predicted at about 24 minutes even for zero pressure if WPS is not effective. The main contributor to this difference in conclusions is believed to be the effective heat transfer coefficient used in the respective analyses. Thus, for cases where the final temperature is in the range of RT_{NDT}, the sensitivity of results to the various input parameters needs to be investigated before final conclusions can be reached as to limiting pressures.

A factor for consideration regarding these transients is that, in general, larger pre-existing cracks are necessary before crack initiation would occur for the cases of higher RT_{NDT}'s. This factor is not illustrated in the figures in this appendix.

This same temperature transient was also analyzed for different values of RT_{NDT} at the vessel inner radius and for a circumferential crack. The results are shown in Figure D-13. Note that the effect of the clad is approximately 8°F and that a circumferential crack will tolerate about a 30°F higher RT_{NDT} (considering crack initiation only) for this transient. Similar variations would be expected for other transients. This example illustrates the benefits to be attained by monotonically decreasing pressure in the event of a moderately severe thermal transient in that it is possible to avoid crack initiation.

For much more severe thermal transients, crack initiation may occur due to high thermal stresses. In this case it is necessary to consider the potential for crack arrest. Figure D-14 is a schematic representation of a critical crack depth diagram to illustrate the analytical model used by the staff for determining acceptable arrest criteria. An upper shelf toughness of 200 ksi $\sqrt{in.}$ is assumed; however, higher or lower values may be more appropriate for a specific reactor vessel. When the thermal stress intensity factor is known at the time of warm prestressing (WPS), the maximum pressure is determined such that arrest will occur at or before the time of WPS and for crack depths below the upper shelf curve. The limiting case is shown as point "A" in the figure. The thermal transient selected for this example is:

$$T_w = 60 + 480\ e^{-\beta t}$$

Figure D-15 illustrates the effect of the cooldown rate with a water to metal heat transfer coefficient of 300 Btu/hr ft^2 °F. Figure D-16 shows the equivalent results for a lower coefficient. Note that the sensitivity to the heat transfer coefficient is greatest for the more rapid cooldown. Figure D-17 shows the effect of various assumptions regarding the attenuation of RT_{NDT} in the metal as discussed in Section 3 and Appendix E of this document. The above figures are for long axial cracks. Figure D-18 shows the effect of assuming long circumferential instead of axial cracks. In terms of RT_{NDT}, instead of being about 30°F for crack initiation, the difference now is about 100°F for crack arrest, depending on the specific pressure. Also shown in Figure D-18 is the effect of crack shape at arrest. (An a/c value of 0.1 represents a crack which is 20 times as long as it is deep.)

Figure D-19 is for another transient. It illustrates the uncertainty in RT_{NDT} that can occur due to the selection of the time of warm prestressing because of the relative flatness of K_I versus time near its peak value. Again, the difference between axial and circumferential cracks is shown when warm prestressing and arrest are considered.

As stated earlier, the NRC staff assumes an infinite flaw length in its analyses; that is, an ellipse with an aspect ratio of zero. For circumferential cracks that arrest at some depth, this assumption is believed to be reasonable if the vessel wall is uniformly coolded in that direction. On the other hand, growing axial cracks could be limited in length by reaching the ends of a critical weld and intercepting tougher plate material. Also, they could extend into less irradiated regions of the vessel wall and hence into tougher materials even within the weld. Thus, the assumptions of an infinitely long axial crack is conservative.

Figure D-20 shows the effect of the assumed crack shape at arrest. If an aspect ratio a/c = 0.1 is assumed instead of zero, there is a gain in RT_{NDT} of about 60° for the case illustrated. This appears to be reasonable in that, if the crack arrested at the ends of an axial weld, it would be approximately half-wall thickness in depth. An assumed aspect ratio of 1/3 would lead to higher tolerable RT_{NDT}'s; however, analyses and experiments related to growing cracks during a severe thermal transient indicate that cracks during a severe thermal transient indicate that cracks arresting with this shape are very unlikely. Also wall penetrations might occur before the ends of the crack reached tough materials. Therefore, the staff does not accept this assumption. If other than infinitely large arresting cracks, say those with an aspect ratio up to 0.1, are to be accepted, then reasonable assumptions have to be made regarding all stresses especially weld residual stresses that can be present in addition to those due to pressure and temperature distribution.

These illustrations are intended to demonstrate the importance of limiting the reactor system pressure in the event of a severe cooldown transient as well as the necessity to allow for uncertainties both in analyses of transients and in material properties. Although WPS is expected o be effective in certain PTS scenarios, this hypothesis ought not to be tested at an operating facility.

Based on the examples illustrated in Section D.6.3 and on the analyses of other organizations for similar PTS scenarios, it is seen that variations in input assumptions can lead to differences of limiting RT_{NDT}'s for axial crack initiation. Specific differences will, of course, depend on specific scenarios. The following are typical results.

	Assumption	Effect on RT_{NDT}, °F
(a)	Clad stress vs. no clad stress	10°
(b)	Continuous flaw for initiation vs. elliptical flaw (a/c = 1/3)	20°
(c)	h = 300 Btu/hr ft^2 °F vs. Westinghouse free convection correlation	15°

The above assumption differences account for a total RT_{NDT} variation of about 45° between staff analyses and those of Westinghouse. The Westinghouse model for fluence attenuation into the wall is equivalent to the dpa model of the staff. Other vendors, however, may still be using other models. The attenuation effect on limiting RT_{NDT}'s for crack initiation is not expected to be great but for crack arrest situations, the difference can be significant as illustrated in Figure D-17.

REFERENCES

D.1. R. Labbens, A. Pellissier-Tanon, J. Heliot, "Practical Method for Calculating Stress-Intensity Factors Through Weight Functions," Mechanics of Crack Growth, ASTM STP-590, American Society for Testing and Materials, 1976, pp. 368-384.

D.2. J. Heliot, R. C. Labbens, A. Pellissier-Tanon, "Semi-Elliptical Cracks in the Mendional Plane of A Cylinder Subjected to Stress Gradients, Calculation of Stress Intensity Factors by the Boundary Integral Equations Method," XIth National Symposium on Fracture Mechanics, Blacksburg, Virginia, June 1978.

D.3. S. K. Iskander, R. D. Cheverton, D. G. Ball, NUREG/CR-2113, ORNL/NUREG-84, OCA-I, "A Code for Calculating the Behavior of Flaws on the Inner Surface of a Pressure Vessel Subjected to Temperature and Pressure Transients."

D.4. A Supplement to ORNL-TM-7931 in Preparation, "PWR Pressure Vessel Integrity During Overcooling Accidents: A Parametric Analysis," Draft dated April 13, 1982.

D.5. "Benefits of Warm Prestressing," by George Irwin, University of Maryland. Minutes of ACRS Briefing on Thermal Shock, December 5, 1981.

D.6. J. J. McGowan, "An Assessment of the Beneficial Effects of Warm Prestressing -Under Severe Thermal Shock," WCAP 9178, March 1975.

D.7. G. L. Guthrie, W. N. McElroy and S. L. Anderson, "A Preliminary Study of the Use of Fuel Management Techniques for Slowing Pressure Vessel Embrittlement," Paper presented at 4th ASTM-Euratom Symposium, March 1982.

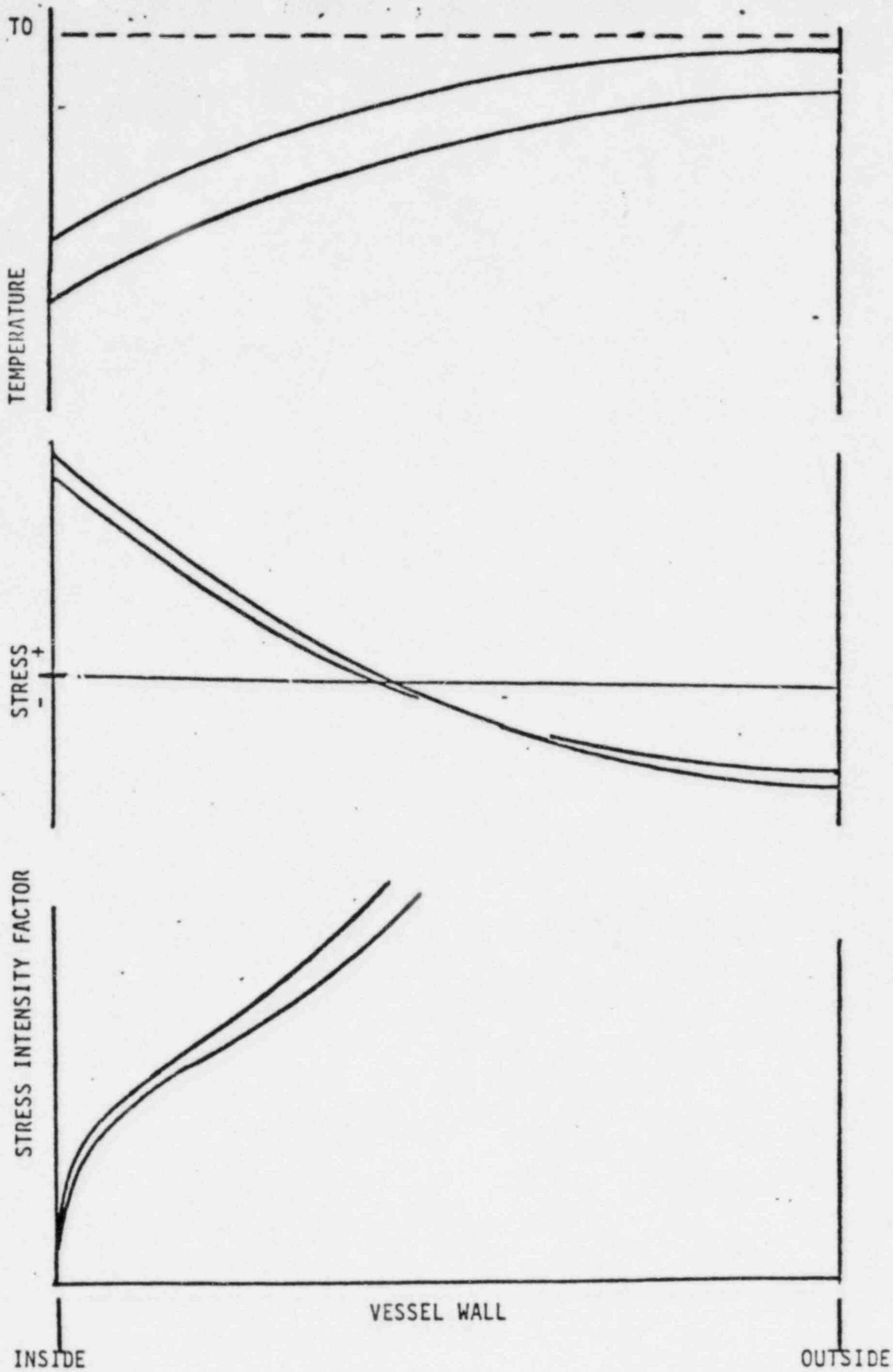


FIGURE D-1
DEVELOPMENT OF PTS TRANSIENT
IN A VESSEL WALL

HYPOTHETICAL PTS TRANSIENT

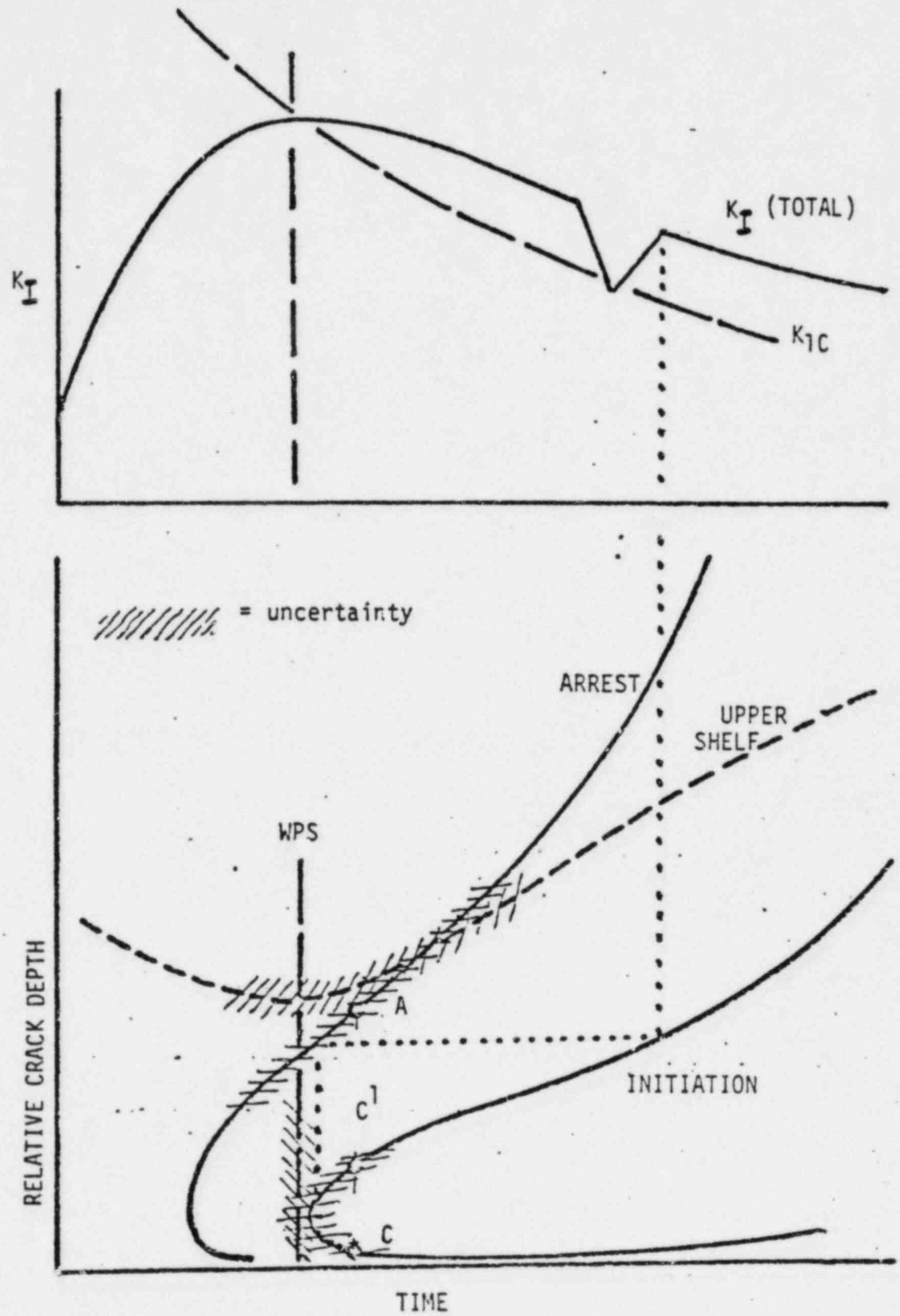


FIGURE D-2

CLAD EFFECTS

RELATIVE COEFFICIENTS
AT 400°F

	SS	SA 533
EXPANSION	10	8
CONDUCTIVITY	10	24

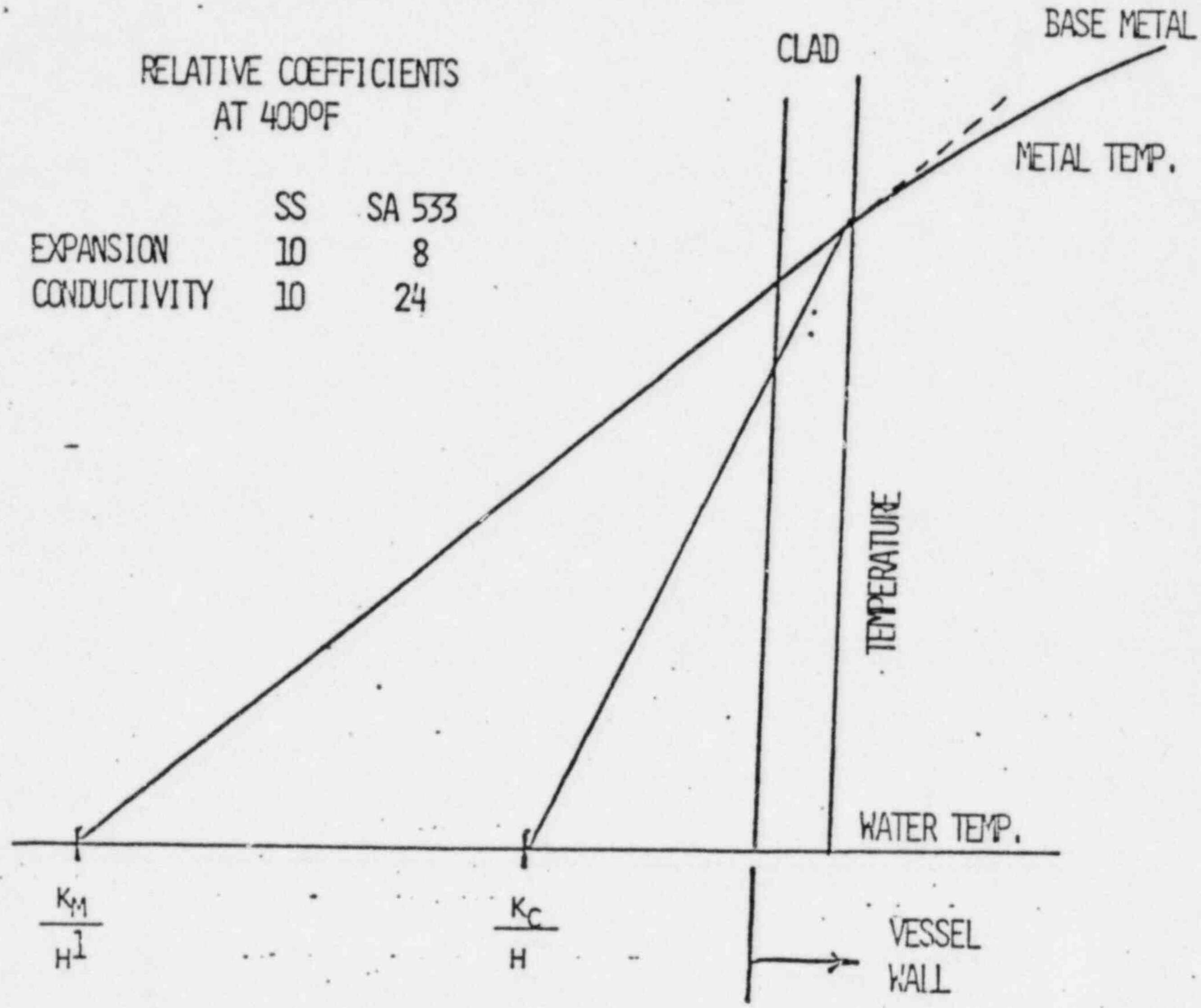
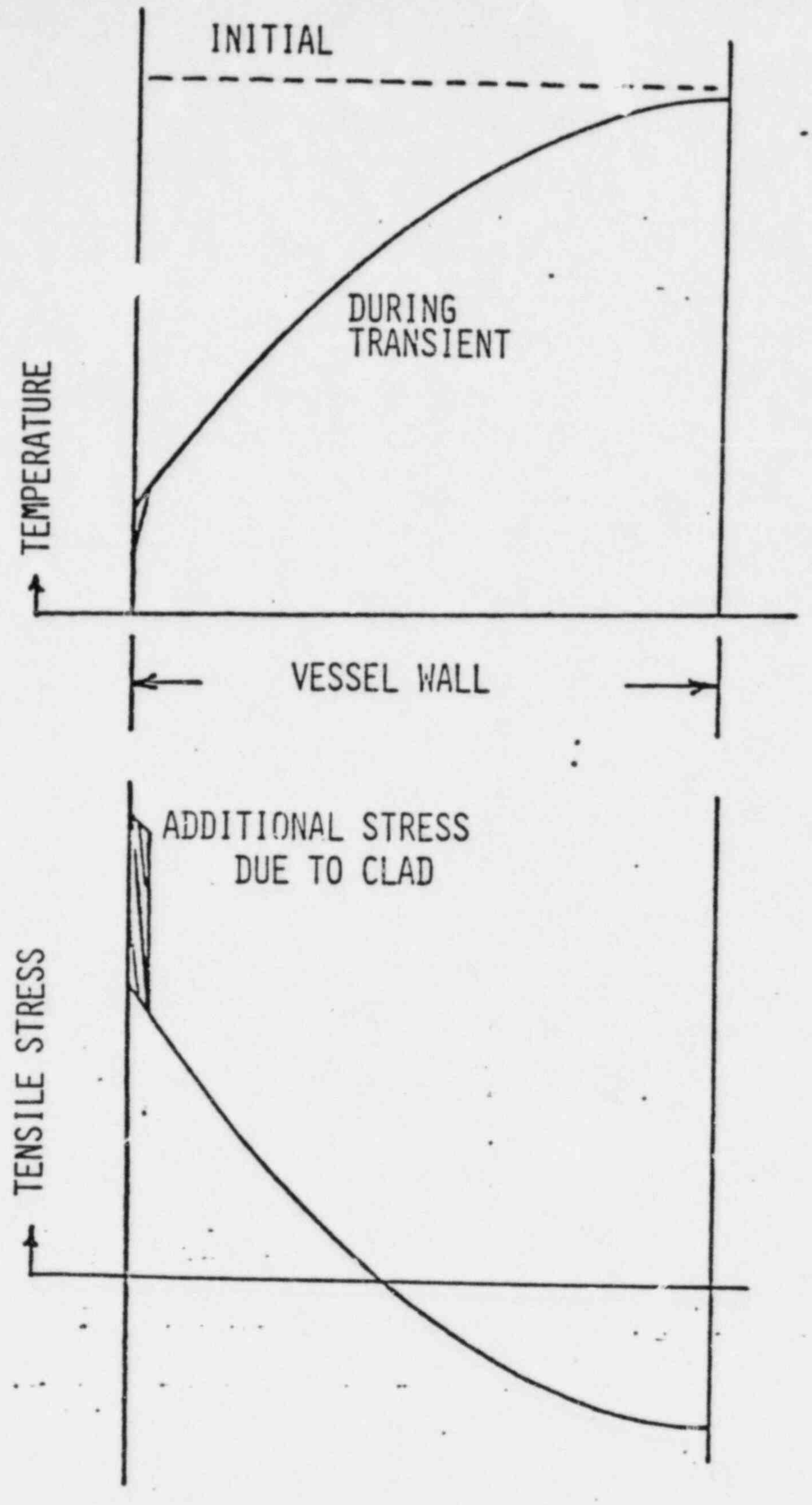
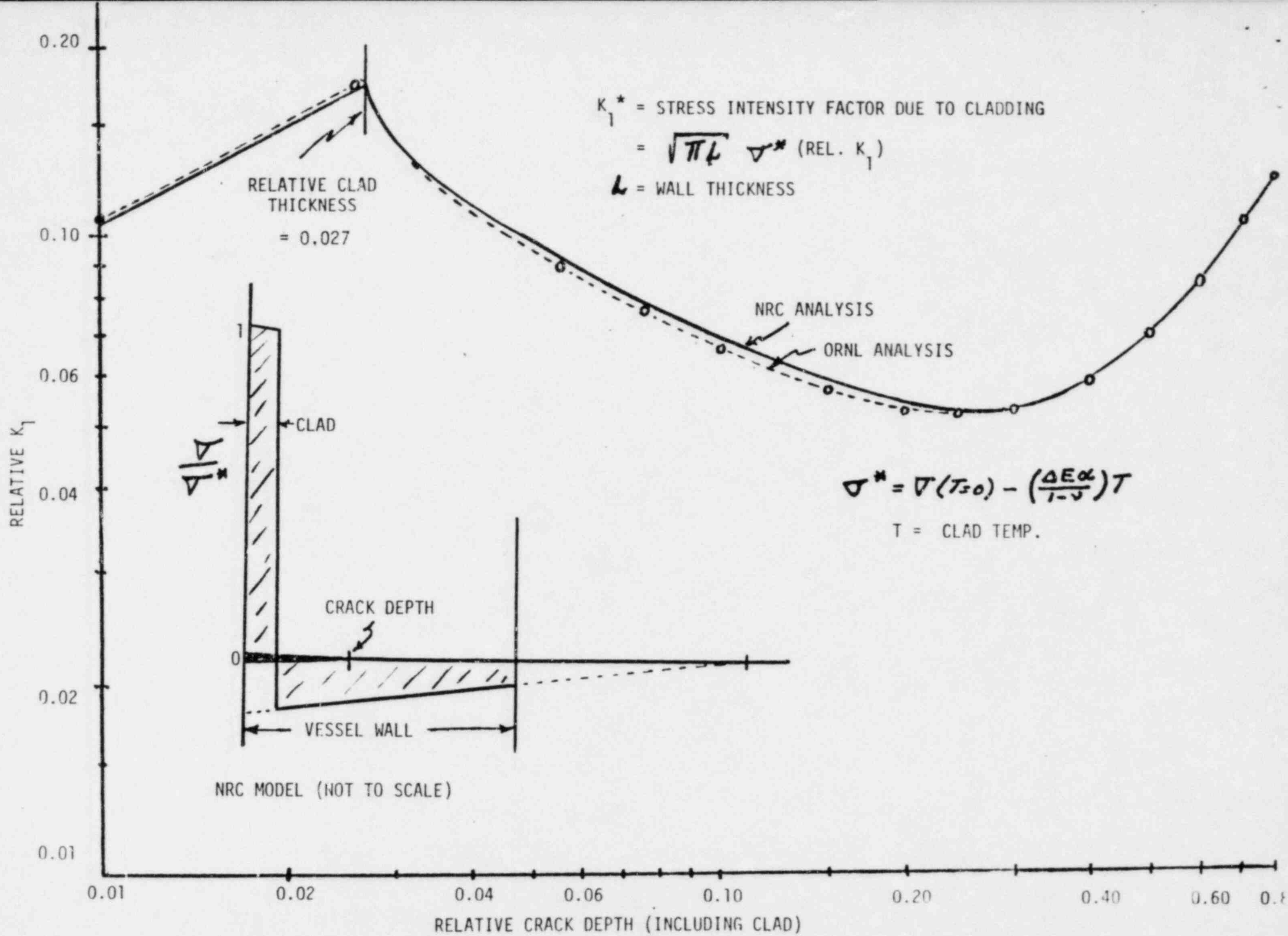


FIGURE D-3



TEMPERATURE AND THERMAL STRESS DISTRIBUTION
DURING A
COOLDOWN TRANSIENT

FIGURE D-4



0.20
0.10
0.06
0.04
0.02
0.01
RELATIVE K_1
K_1^* = STRESS INTENSITY FACTOR DUE TO CLADDING
$= \sqrt{\pi t}\ \sigma^*$ (REL. K_1)
t = WALL THICKNESS
RELATIVE CLAD THICKNESS = 0.027
NRC ANALYSIS
ORNL ANALYSIS
$\sigma^* = \sigma(T=0) - \left(\frac{\Delta E \alpha}{1-\nu}\right) T$
T = CLAD TEMP.
1
0
$\frac{\sigma}{\sigma^*}$
CLAD
CRACK DEPTH
VESSEL WALL
NRC MODEL (NOT TO SCALE)
0.01
0.02
0.04
0.06
0.10
0.20
0.40
0.60
RELATIVE CRACK DEPTH (INCLUDING CLAD)

FIGURE D-5

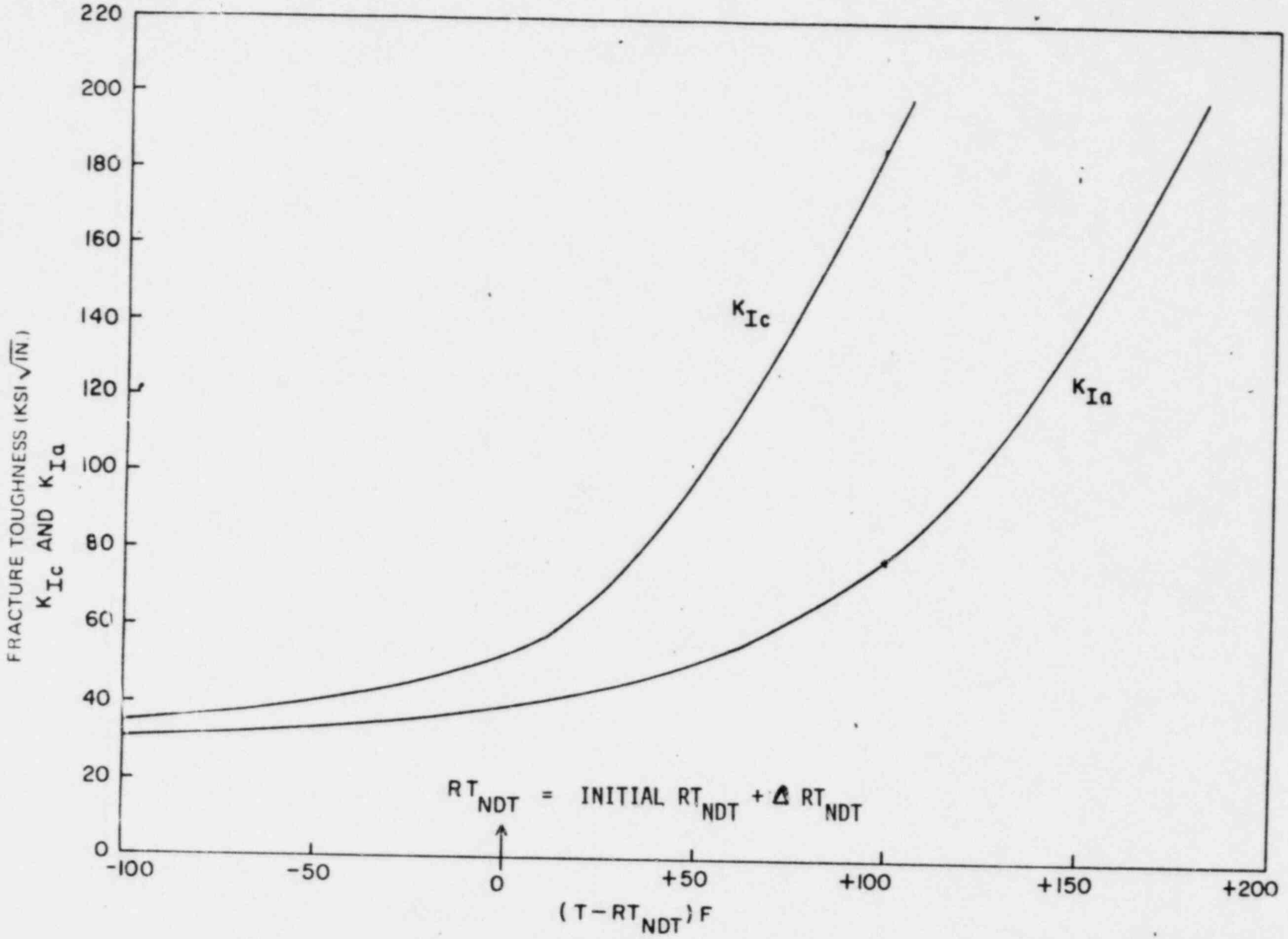
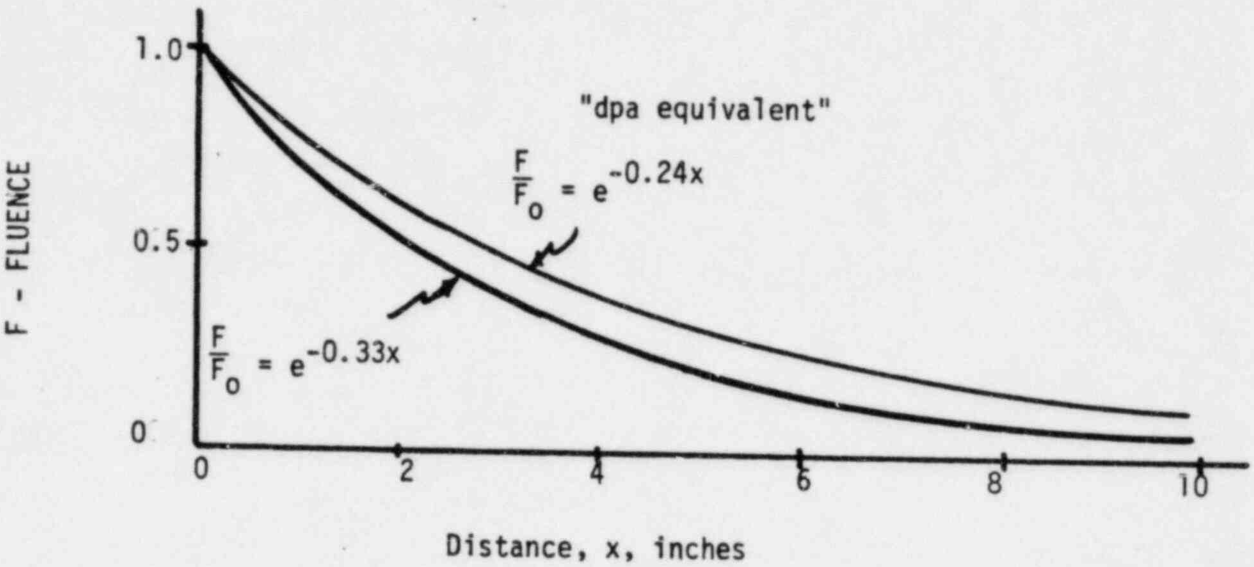


FIG. A-4200-1 LOWER BOUND K_{IA} and K_{IC} TEST DATA FOR SA-533 GRADE B CLASS 1, SA-508 CLASS 2, AND SA-508 CLASS 3 STEELS

FIGURE D-6, ASME CODE SECTION XI CURVES FOR RELATING FRACTURE TOUGHNESS TO $T-RT_{NDT}$



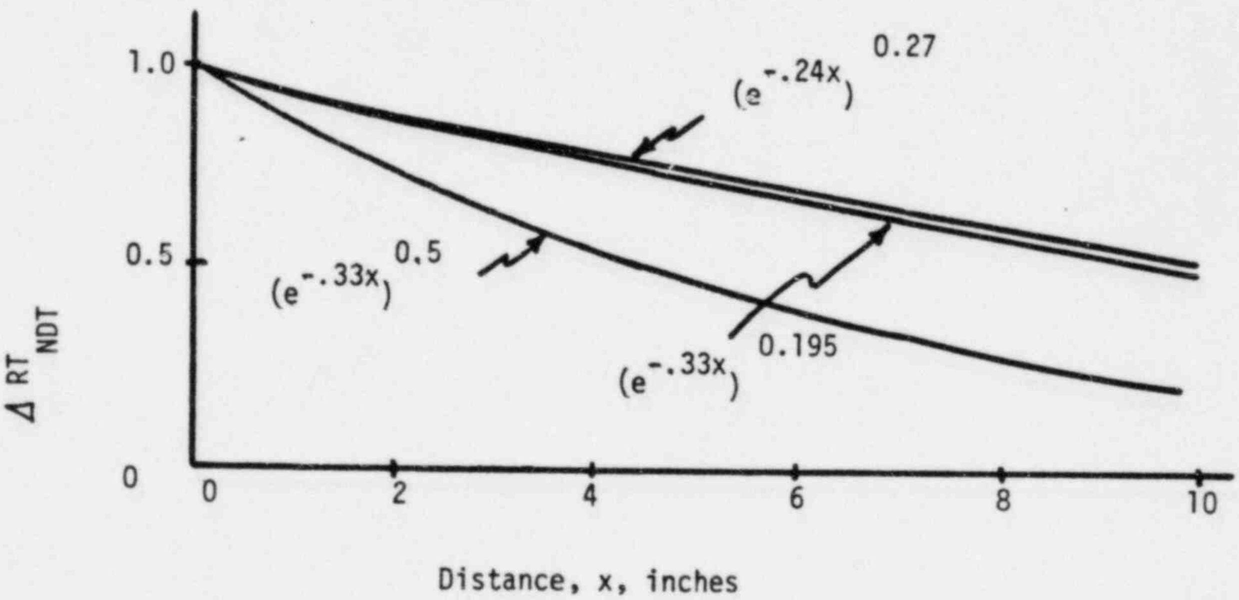
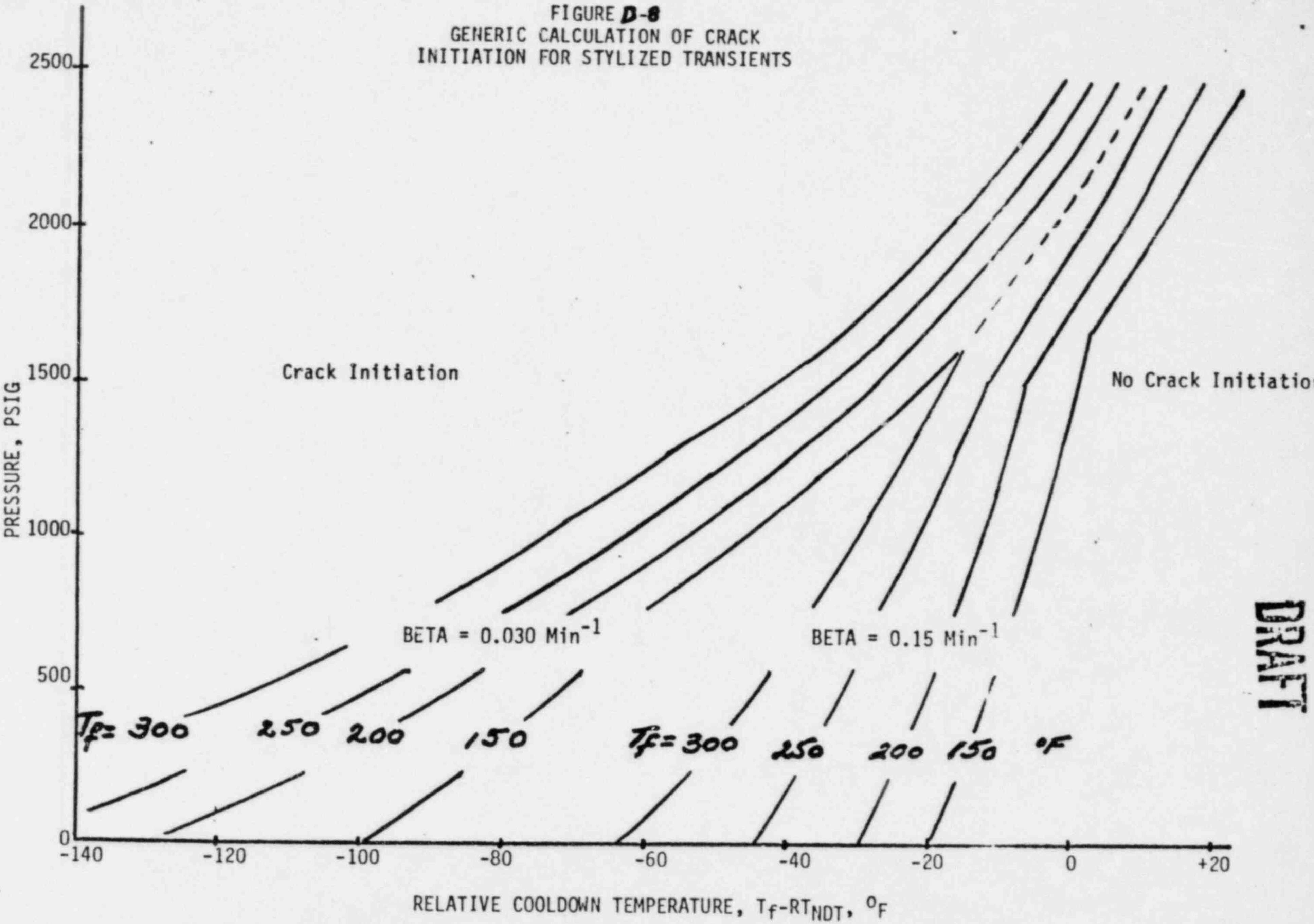


FIGURE D-7, ATTENUATION OF FLUENCE AND ΔRT_{NDT} THROUGH THE VESSEL WALL

FIGURE D-8
GENERIC CALCULATION OF CRACK
INITIATION FOR STYLIZED TRANSIENTS



DRAFT

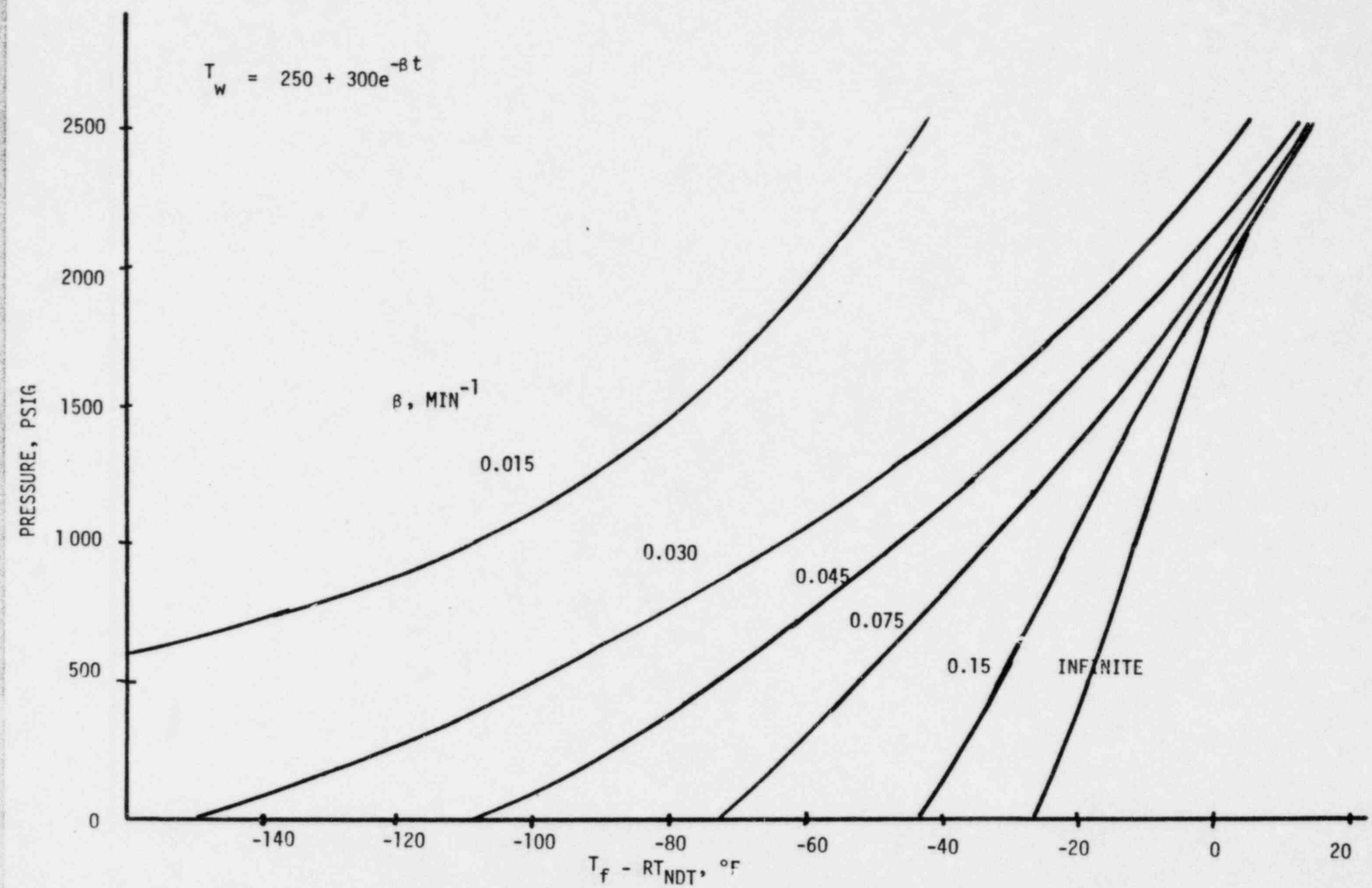


FIGURE D-9 EFFECT OF THE RECIPROCAL TIME CONSTANT, β, ON THE CRITICAL VALUES OF PRESSURE AND $T_f - RT_{NDT}$ FOR CRACK INITIATION

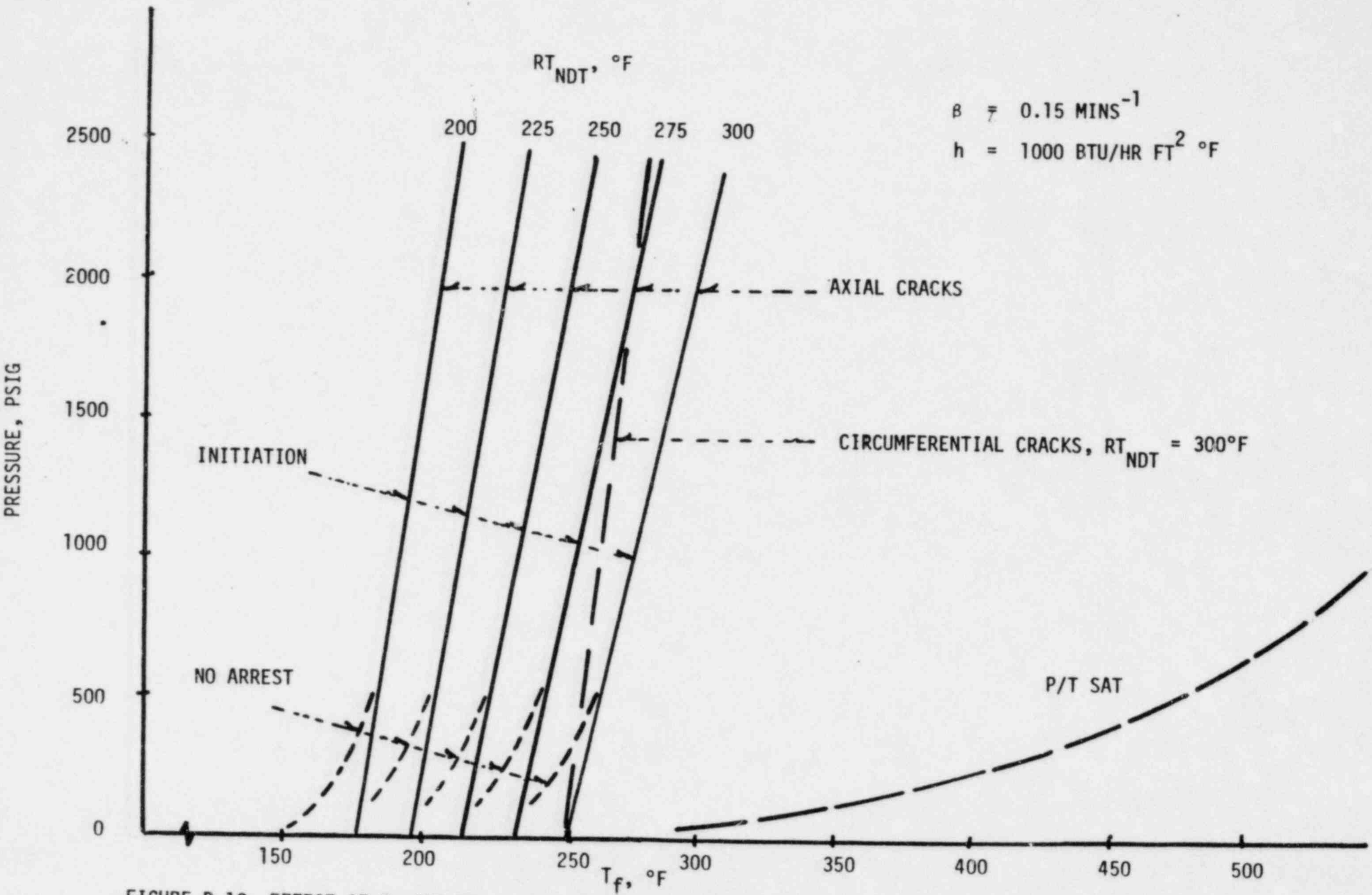


FIGURE D-10 EFFECT OF T_f AND RT_{NDT} ON THE CRITICAL PRESSURE

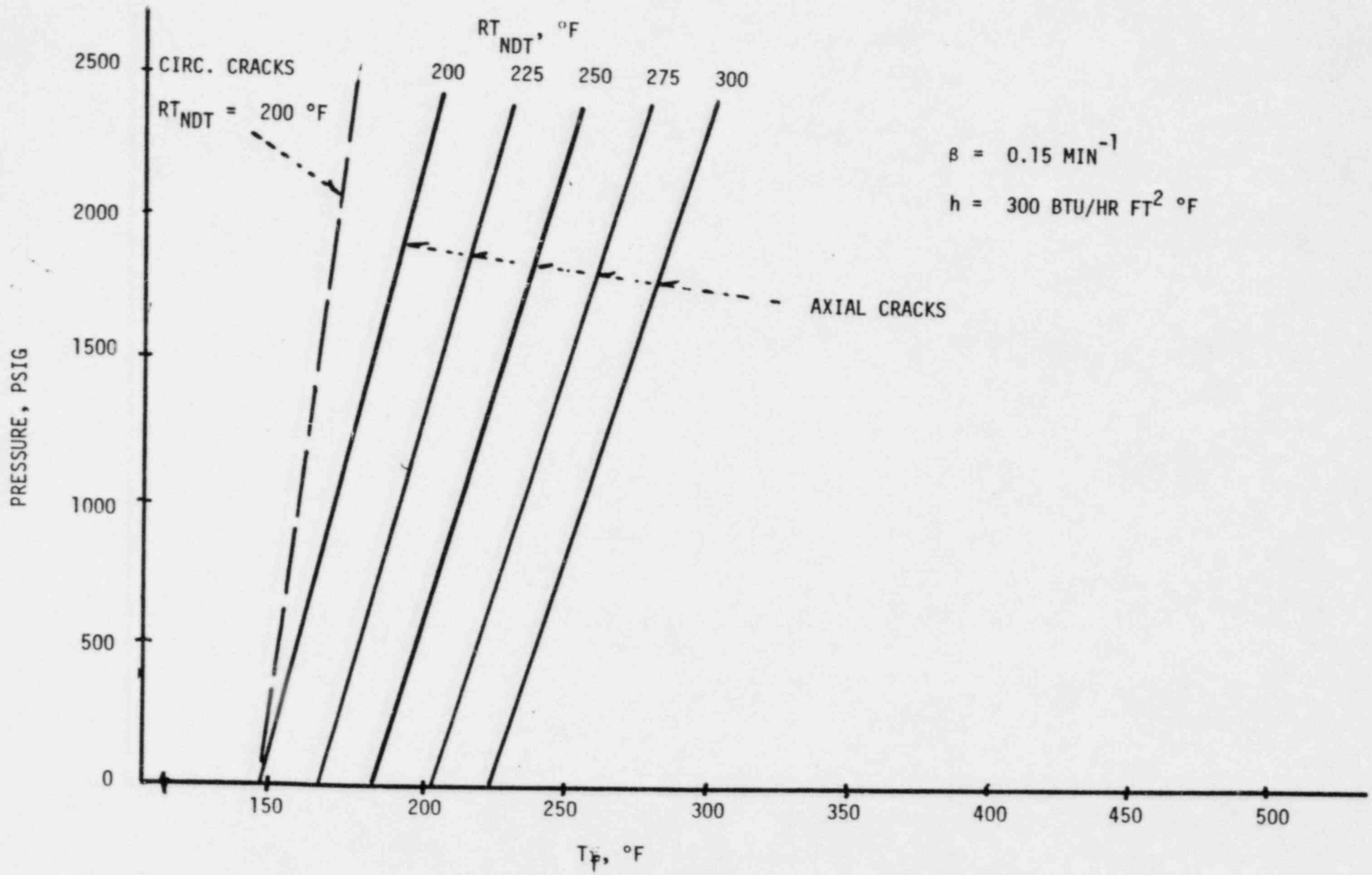


FIGURE D-11 EFFECT OF T_f AND RT_{NDT} ON THE CRITICAL PRESSURE FOR CRACK INITIATION

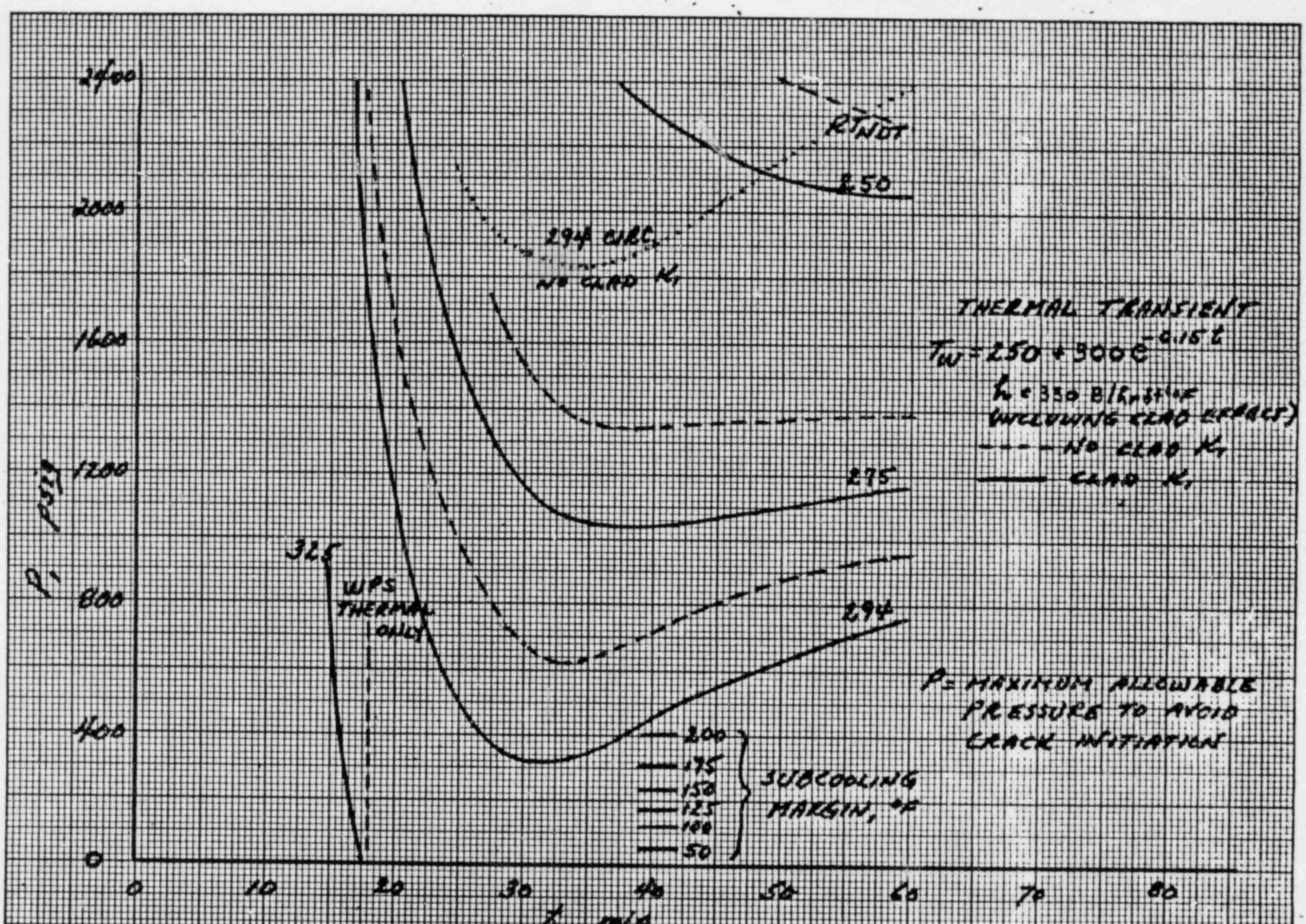

2400
2000
1600
1200
800
400
0
P, PSIG
0
10
20
30
40
50
60
70
80
t, min.
RT NDT
250
294 CIRC.
NO CLAD K1
275
294
325
WPS THERMAL ONLY
THERMAL TRANSIENT
TW = 250 + 300e^-0.15t
---- NO CLAD K1
—— CLAD K1
P= MAXIMUM ALLOWABLE PRESSURE TO AVOID CRACK INITIATION
200
175
150
125
100
50
SUBCOOLING MARGIN, °F

FIGURE D-12

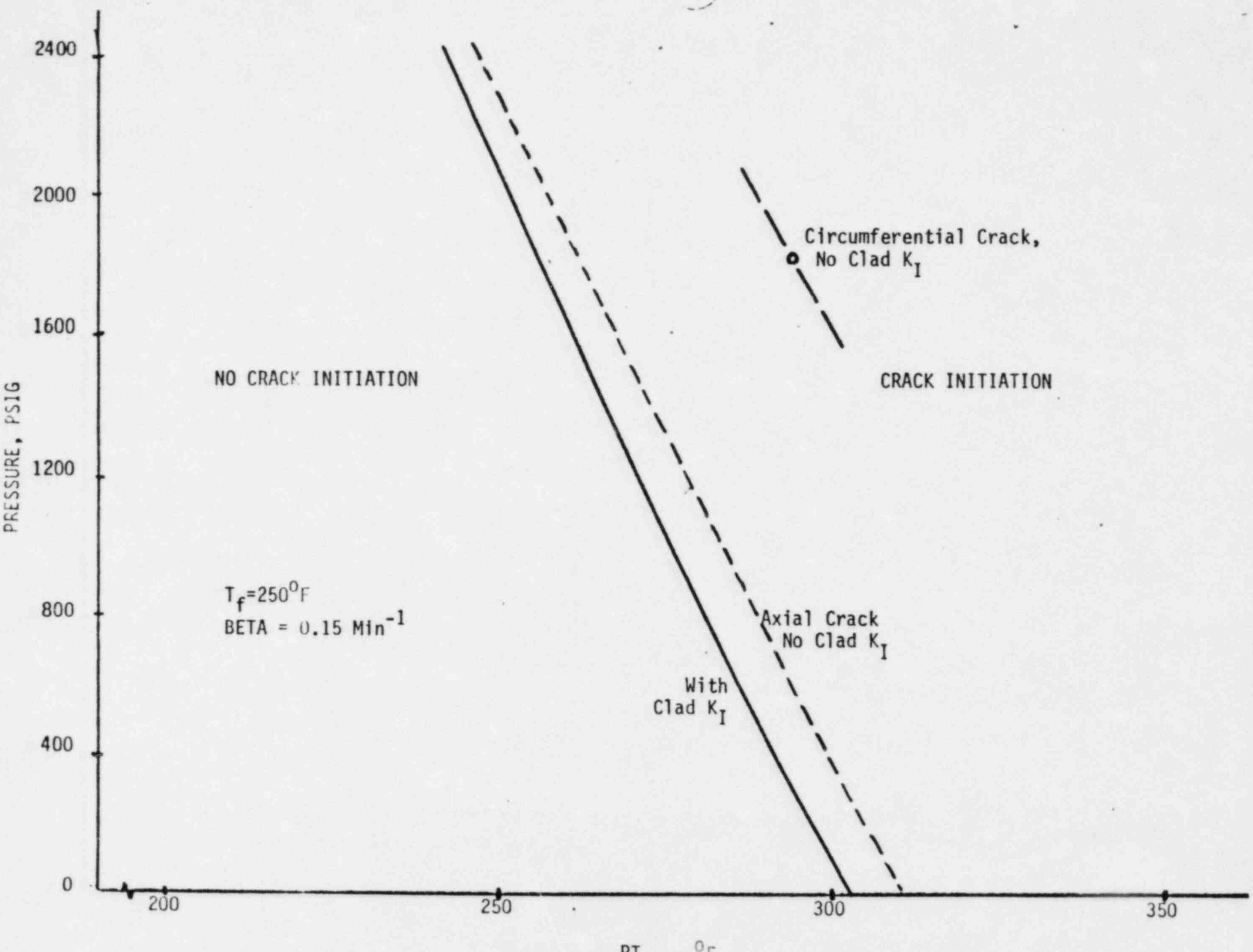


FIGURE D-13 SENSITIVITY STUDIES

DRAFT

DRAFT

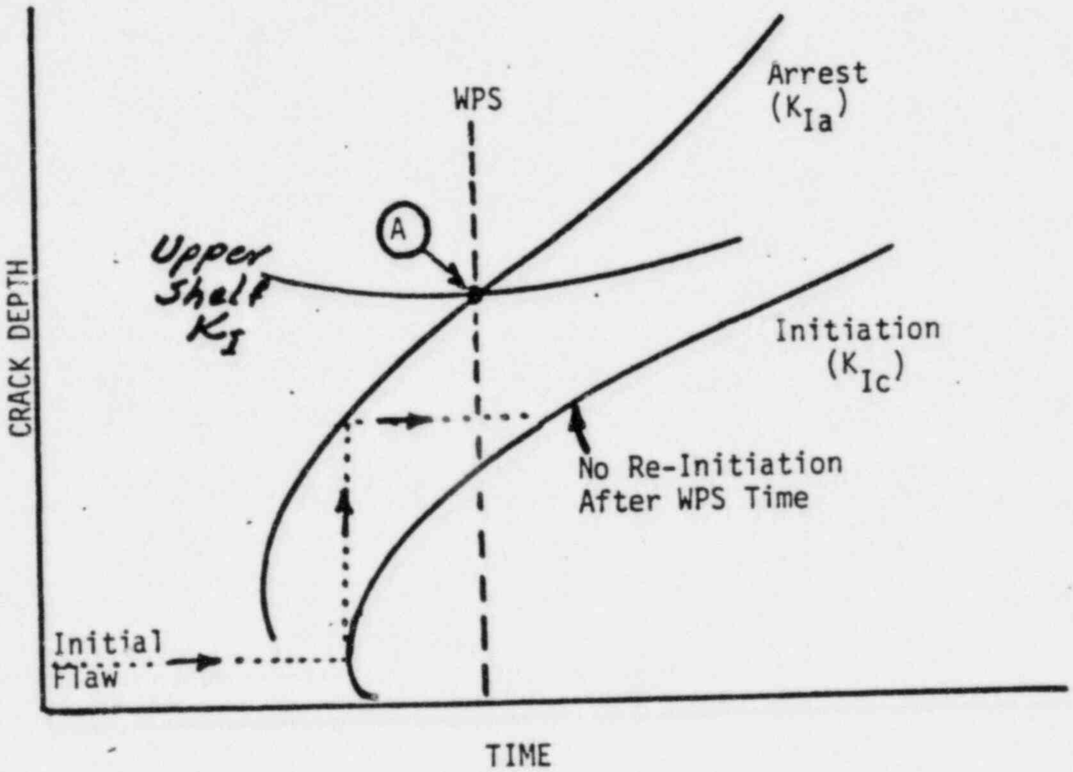
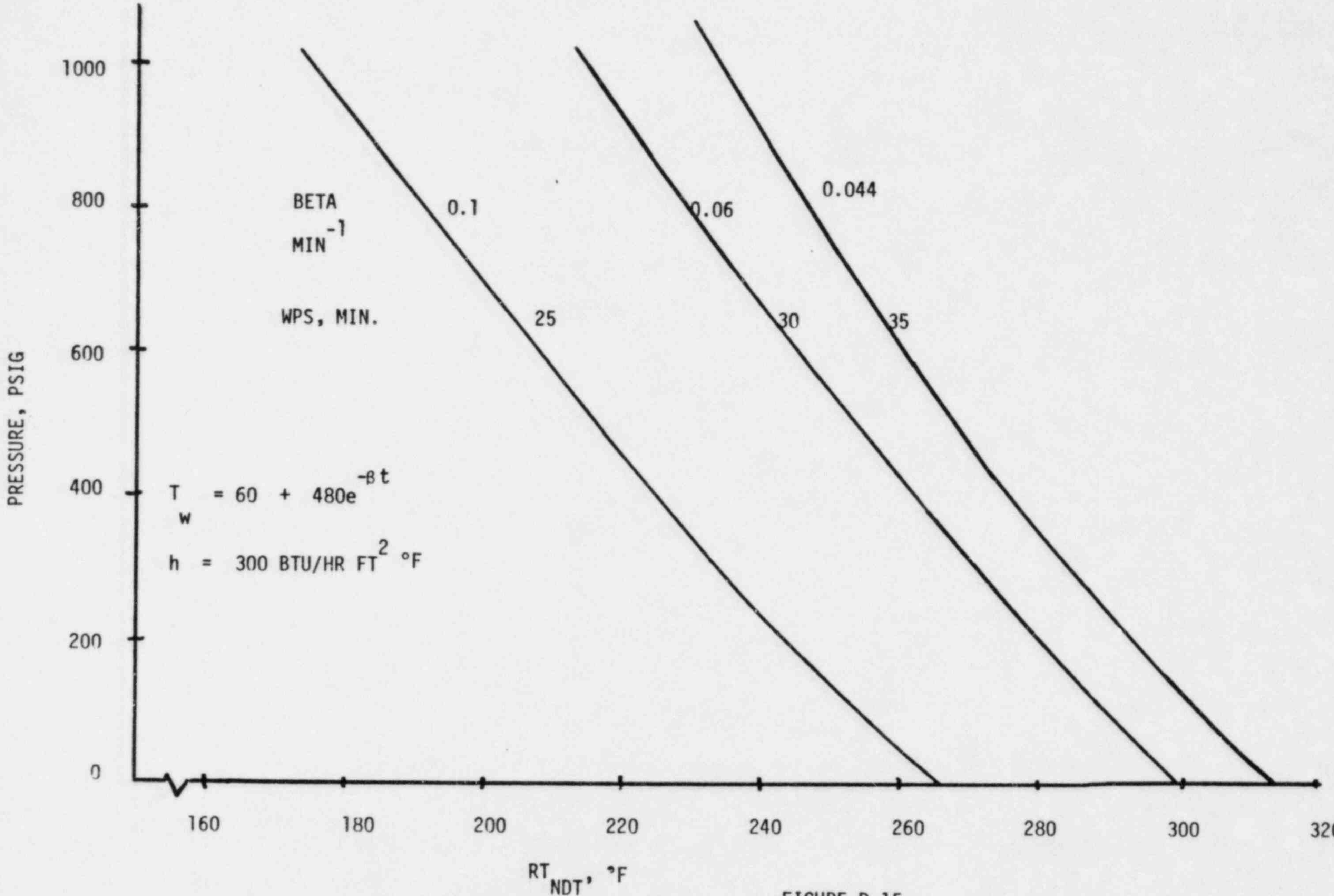


FIGURE D-[illegible] NRC CRACK
INITIATION AND ARREST
MODEL



PRESSURE, PSIG
1000
800
600
400
200
0
BETA
MIN^-1
WPS, MIN.
0.1
0.06
0.044
25
30
35
T_w = 60 + 480e^-βt
h = 300 BTU/HR FT^2 °F
160
180
200
220
240
260
280
300
320
RT_NDT, °F

FIGURE D-15

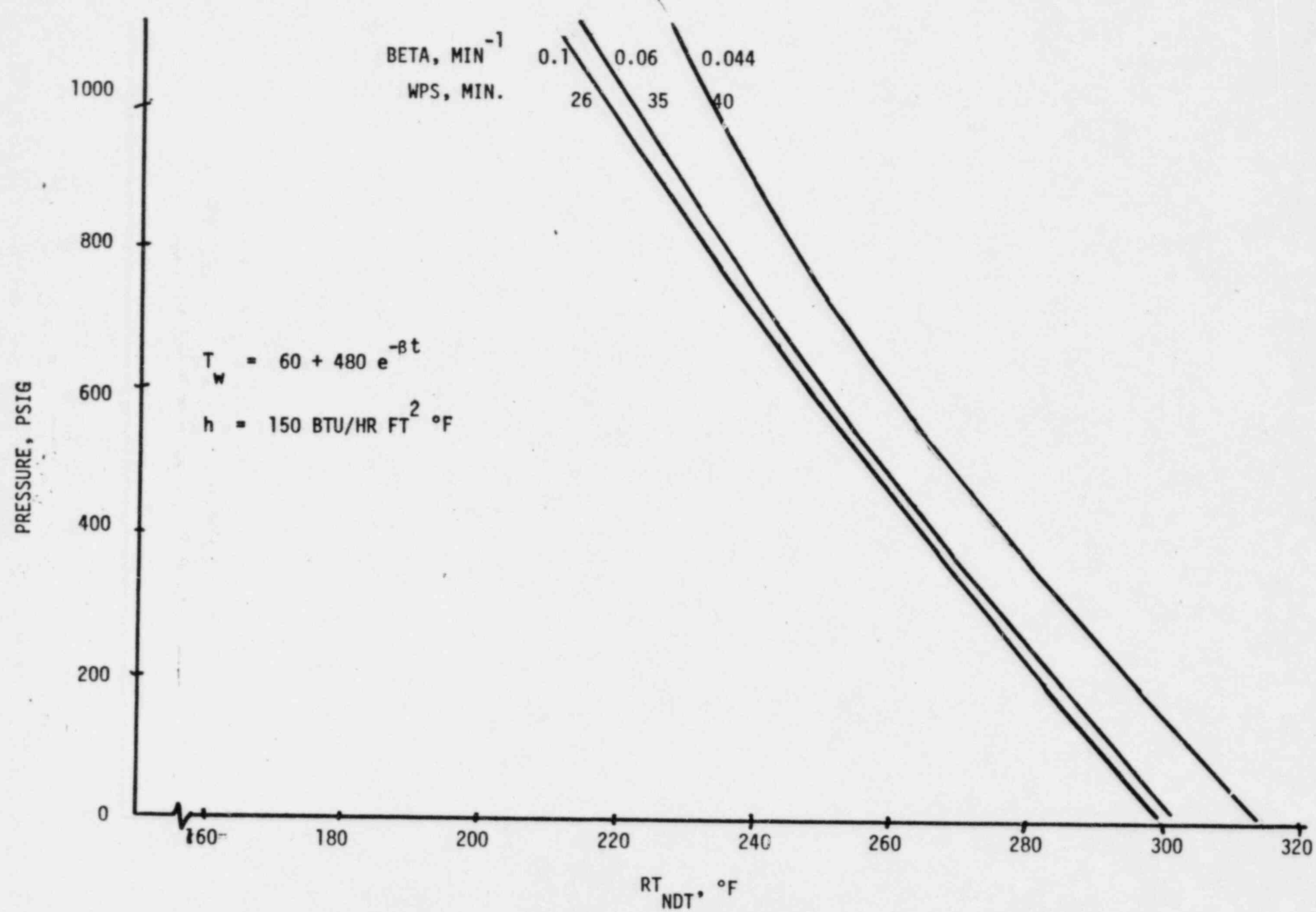
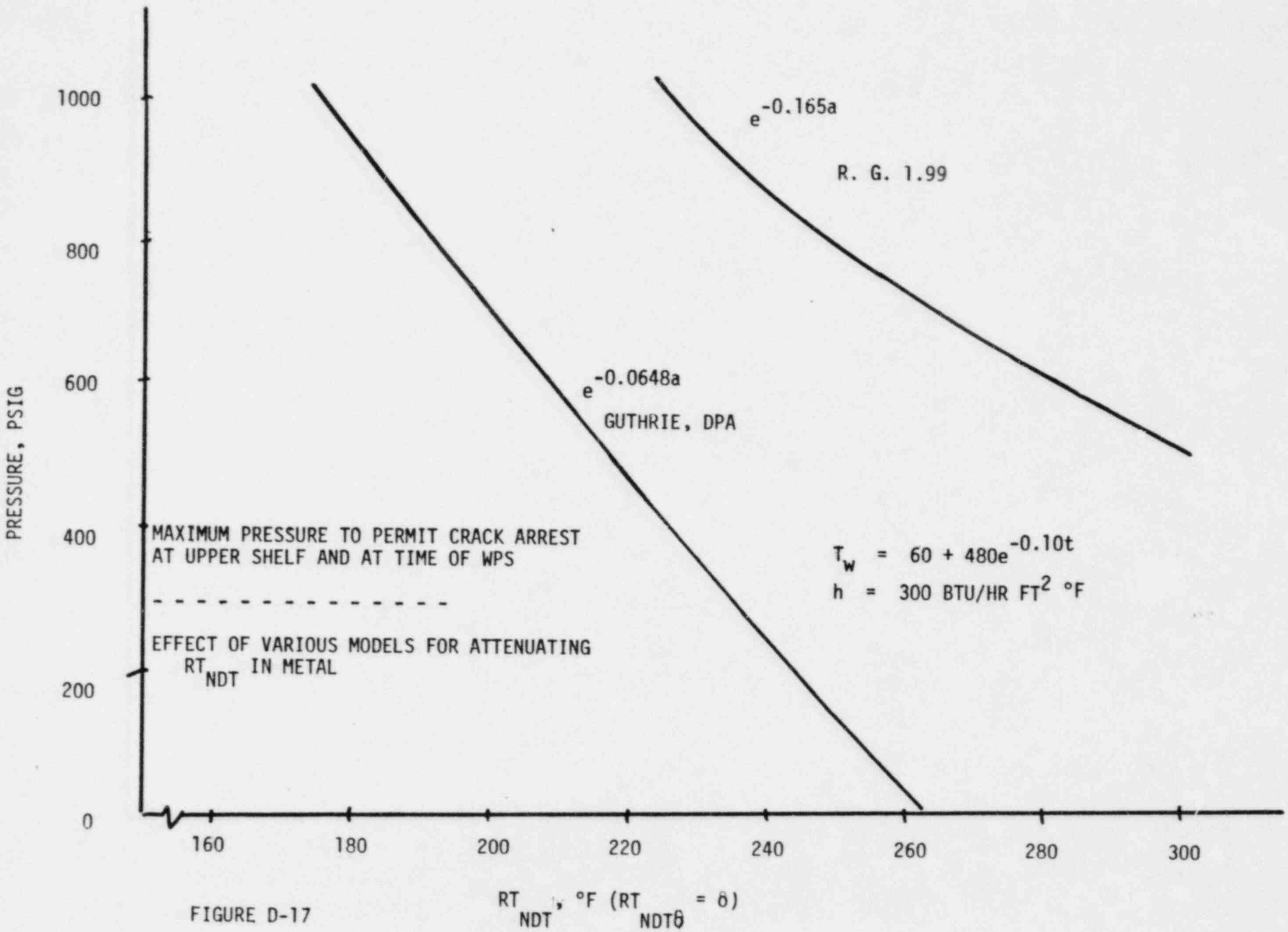
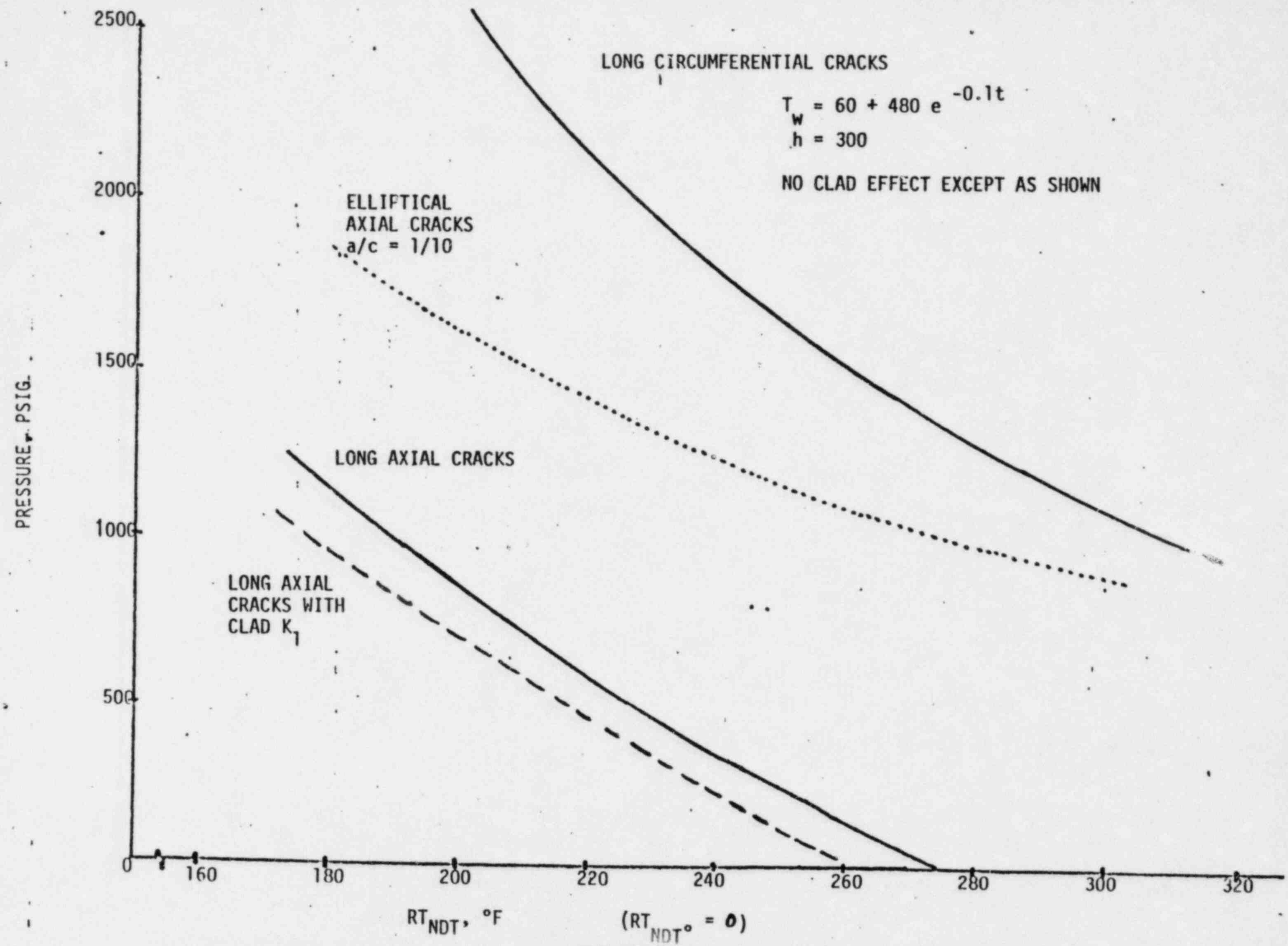

BETA, MIN^{-1} 0.1 0.06 0.044
WPS, MIN. 26 35 40
$T_w = 60 + 480 e^{-\beta t}$
$h = 150$ BTU/HR FT^2 °F
PRESSURE, PSIG
1000
800
600
400
200
0
160
180
200
220
240
260
280
300
320
RT_{NDT}, °F

FIGURE D-16



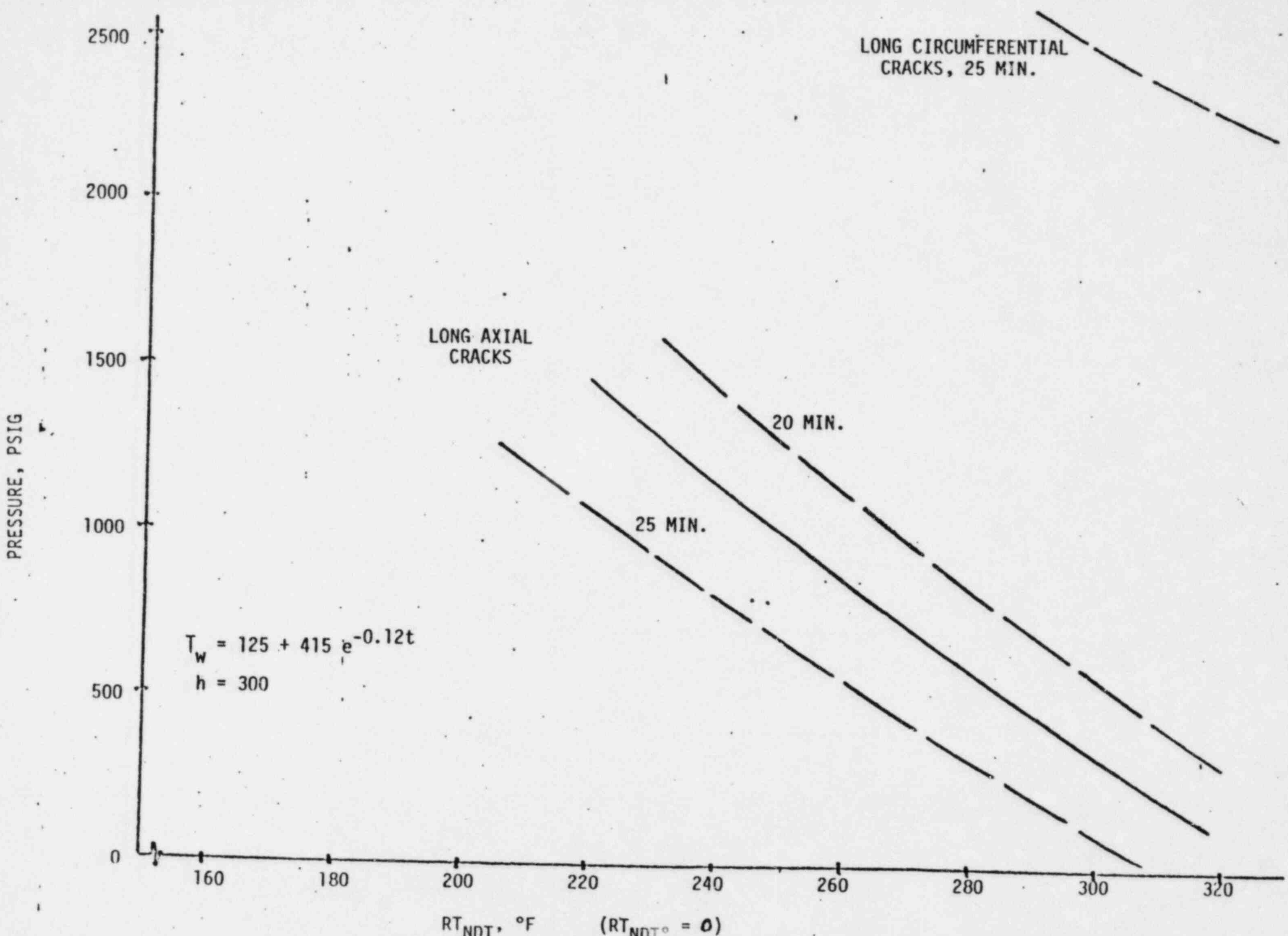
PRESSURE, PSIG
1000
800
600
400
200
0
160
180
200
220
240
260
280
300
$e^{-0.165a}$
R. G. 1.99
$e^{-0.0648a}$
GUTHRIE, DPA
MAXIMUM PRESSURE TO PERMIT CRACK ARREST AT UPPER SHELF AND AT TIME OF WPS
EFFECT OF VARIOUS MODELS FOR ATTENUATING RT_{NDT} IN METAL
$T_w = 60 + 480e^{-0.10t}$
$h = 300$ BTU/HR FT^2 °F
RT_{NDT}, °F ($RT_{NDT\theta} = 0$)

FIGURE D-17



LONG CIRCUMFERENTIAL CRACKS
$T_w = 60 + 480\ e^{-0.1t}$
$h = 300$
NO CLAD EFFECT EXCEPT AS SHOWN
ELLIPTICAL AXIAL CRACKS a/c = 1/10
LONG AXIAL CRACKS
LONG AXIAL CRACKS WITH CLAD K_I
PRESSURE, PSIG.
2500
2000
1500
1000
500
0
160
180
200
220
240
260
280
300
320
RT_{NDT}, °F
(RT_{NDT° = 0)

FIGURE D-18



LONG CIRCUMFERENTIAL
CRACKS, 25 MIN.
LONG AXIAL
CRACKS
20 MIN.
25 MIN.
$T_w = 125 + 415\, e^{-0.12t}$
$h = 300$
PRESSURE, PSIG
2500
2000
1500
1000
500
0
160
180
200
220
240
260
280
300
320
RT_{NDT}, °F (RT$_{NDT°}$ = 0)

FIGURE D-19

EFFECT OF ASPECT RATIO

AT CRACK ARREST

EXAMPLE

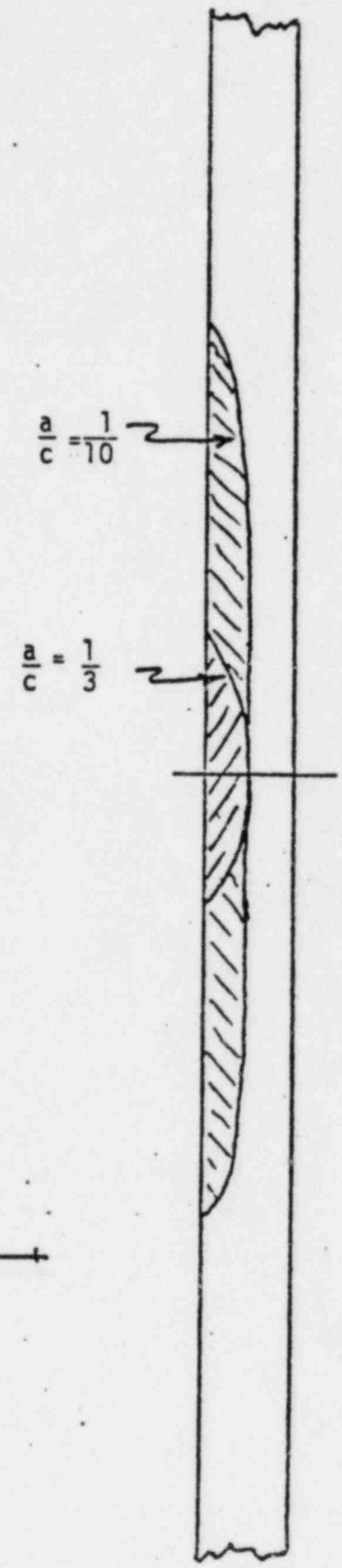
$P = 1250$ psig

$T_W = 60 + 480\ e^{-0.1t}$

$h = 300$

no clad effect

$t = 25$ min. (WPS)



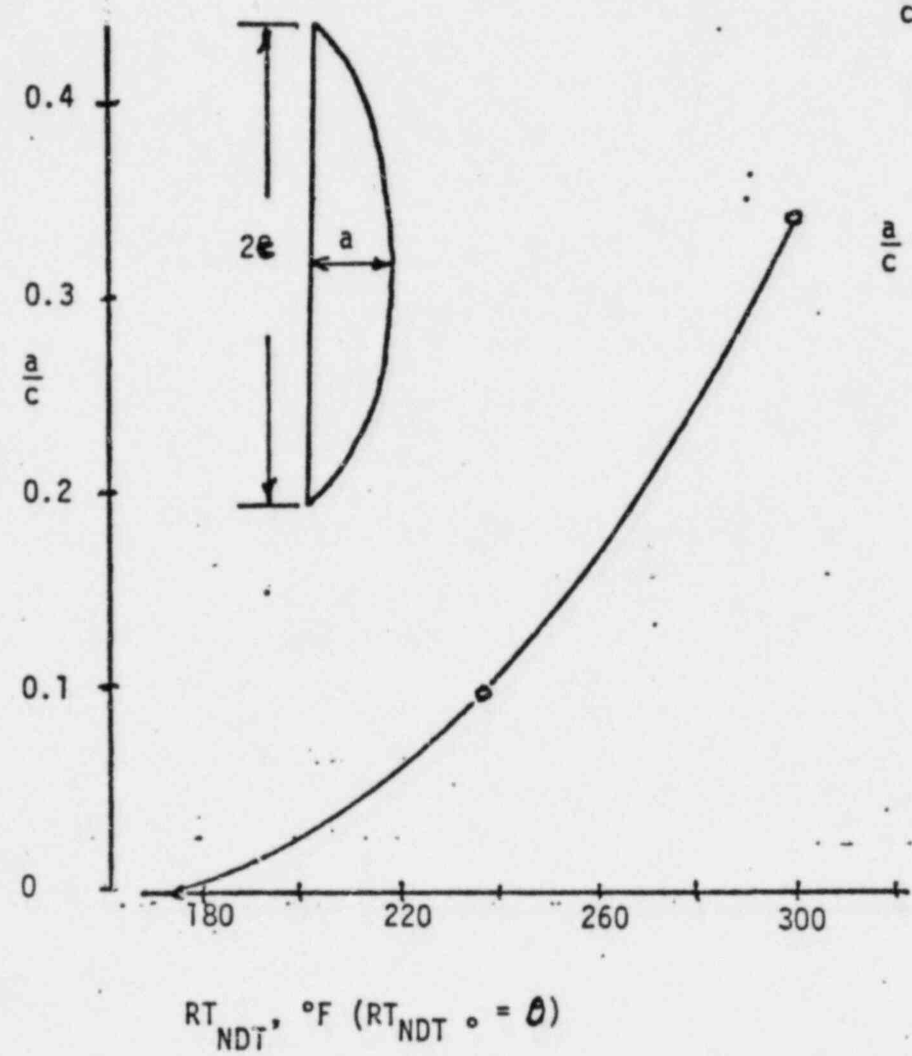


FIGURE D-20

APPENDIX E

DETERMINATION OF RT_{NDT} FOR PLANTS FOR COMPARISON WITH SCREENING CRITERIA

E.1 Introduction

In Section 3, RT_{NDT} was shown to be an important quantity in the fracture analysis of PTS, because the toughness values, K_{Ic} and K_{Ia} are given in the ASME Code, Section XI as a function of $T - RT_{NDT}$. (In such analyses, the metal temperature, T, and the adjusted reference temperature, RT_{NDT}, are the values at the tip of the postulated crack.) Moreover, the results of the parametric studies described in Sections 3 and 7 and Appendixes D and H show that $T_f - RT_{NDT}$ is an important factor in the characterization of cooldown transient severity for a given plant. In this case, T_f is the asymptotic cooldown temperature of the water in the downcomer, and RT_{NDT} is estimated at the inside surface of the vessel. This finding led to consideration of RT_{NDT} as a screening criterion. Obviously, RT_{NDT} for a given plant is not related to the severity or probability of occurrence of a PTS in that plant and is therefore not necessarily the overall criterion for rating plants. Nevertheless, the value of RT_{NDT} at the inside surface of the vessel is a good screening criterion for the tendency of a reactor vessel to suffer damage from PTS.

RT_{NDT} is the sum of two quantities: the initial RT_{NDT} from tests made at the time the vessel was fabricated and the ΔRT_{NDT} estimated from tests designed to measure the effects of neutron radiation. The purpose of this discussion is to describe the bases for estimated initial RT_{NDT} and ΔRT_{NDT} for the individual plants. Estimates will be given for the inside surface of the vessel wall (at the clad-base metal interface) for the critical locations, either a longitudinal weld or a circumferential weld in the beltline or occasionally a beltline plate or forging.

As described below, there are a number of uncertainties in the estimation of initial RT_{NDT} and ΔRT_{NDT}, and thus there is the difficult question of establishing a proper, known, degree of conservatism in the estimate of RT_{NDT}. To resolve this question, a Working Group on RT_{NDT} was assembled for a two-day meeting (June 17 and 18, 1982) to review the NRC methods and recommend a method for use in the report. The work of that group is described in Reference 10. The method described below follows the recommendations of the Working Group.

E.2 Initial RT_{NDT}

E.2.1 Code Definition

The Summer 1972 Addenda to Section III of the ASME Boiler and Pressure Vessel Code contained the first requirements for measurement of RT_{NDT} for the plates, forgings, and welds that make up the reactor vessel, measurements to be made at the time of fabrication. Two types of tests are required--drop-weight tests and Charpy tests. However, most of the vessels in question were fabricated in the 1960's when only Charpy tests were required.

E.2.2 Absence of Actual Measurements of RT_{NDT}

Typically, the data available to the NRC staff comprise 3 Charpy tests at 10°F for each plate, forging and weld, complete Charpy curves for the surveillance weld and base materials, and in cases where the base material was controlling, some drop weight data on archive or surveillance material. In the past, the NRC has used the guidelines of Branch Position MTEB 5-2 to obtain an estimate of initial RT_{NDT}. In summary, those guidelines were to use the Charpy 30 ft. lb. level, but not lower than 0°F. The Charpy curves from the surveillance tests were used to guide any extrapolation needed to get the 30 ft. lb. temperature from the 3 tests results at +10°F.

In summary, values of initial RT_{NDT} measured according to ASME Code rules are not generally available for the welds in question. Estimates based on the 3 Charpy test results and MTEB 5-2 are not very satisfactory, because they are overconservative for some cases.

E.2.3 Generic Data

From compilations of data obtained subsequent to the time the vessels in question were made, it is possible to divide the welds into two groups according to the weld flux used, and to develop a mean value and a standard deviation for the generic data. One must then decide if it is prudent to use the mean generic value as the best estimate for the vessel welds in question. Except for some archive material, the welds that are represented in the data base were made at a later time than the vessel welds. There may have been some differences in weld chemistry or welding practice. Furthermore, even if there were actual RT_{NDT} values for the vessel weld in question, the samples would come from weld metal qualification welds, not from actual vessel weld prolongations and not from full thickness test pieces. Thus, a mean plus 2 sigma value appears to be the best engineering estimate for initial RT_{NDT} for use in a screening criterion.

In the Combustion Engineering Report, CEN-189 (Ref. E.2) there is a table of values of initial RT_{NDT} which contains 49 values for Linde 0091, 20 values for Linde 124, and 13 values for unidentified weld fluxes, some of which we have identified as Linde 1092. By inspection, the three groups appear to be in the same population, and the total has been treated as such to yield a mean value of -56°F and a standard deviation of 17°F. It was pointed out by PNL (Ref. E.3) that these data are not normally distributed, but are skewed to the high side. However, the resulting error is swamped by the uncertainty in the application of these data to the actual vessels. An earlier weld flux, ARCOS B-5, used on one or two vessels, was deemed to be in the same population based on comparison of available Charpy energy values.

For Linde 80 weld flux, a set of 10 values provided by Babcock and Wilcox (Ref. E.4) had a mean value of 0°F and the range was from -40° to +20°F. Because the sample size for the Linde 80 welds was small, the standard deviation was taken to be the same as for Linde 0091 welds, 17°F.

E.2.4 Comparison with Vendor's Values

Westinghouse (WCAP 10019) (Ref. E.1) used MTEB 5-2 to estimate RT_{NDT} values. Combustion Engineering (CE) (Ref. E.2) proposed two bases: (1) 60°F below the

Charpy 50 ft. lb. level, and (2) an upper-2-sigma value from generic data for the weld fluxes in their vessels, Linde 0091, 1092, and 124. Their utilities used method 1, but the CE report made for each plant used method 2. Babcock and Wilcox (B&W) used upper bound values from generic data for Linde 80 weld flux, which was used in their vessels. An exception is Three Mile Island 1, which used a lower value of initial RT_{NDT}, basis not specified.

The following table compares vendors' values with NRC values. The latter are mean plus two sigma values. As described in paragraph E.4, in combining initial and ΔRT_{NDT}, the full two sigma value is reduced about 10 degrees by the use of the quantity $2\sqrt{\sigma_o 2 + \sigma_\delta^2}$.

	Linde 80 flux	Linde 0091 etc. flux
NRC	0°F mean Plus 34 = 34°F	-56°F mean Plus 34 = -22°F
W	0 to +10°F	0 to +10°F
CE		-20°
CE Utilities		-50°F
B&W	+20°F	

E.3 Adjustment of RT_{NDT} Due to Radiation (ΔRT_{NDT})

E.3.1 Trend Curves versus Surveillance

Most of the plants in question in the thermal shock issue have withdrawn at least one surveillance capsule and tested the irradiated specimens therein. The fluence is generally not exactly the value of interest, but the results can be extrapolated to the fluence of interest by using one of the trend curves to be described.

However, there are problems associated with using individual surveillance results as the sole source of information about a plant. First, the surveillance weld often does not match the critical vessel weld exactly, i.e., the weld wire heat numbers are different. A broader problem is that caused by scatter in the ΔRT_{NDT} data. This results in part from the fact that ΔRT_{NDT} is the difference between the curves for irradiated and unirradiated material, both of which were fitted to data that typically shows considerable scatter.

Thus, there is a preference for the use of trend curves instead of individual surveillance data. To use any of the trend curves, the chemistry of the material must be known, in particular, the copper content. This is obtained from analysis of the weld metal qualification weld for the weld wire heat number and weld flux number that were used for the critical weld. If not available, data were sought for that weld wire heat number as used in other vessels. Failing that, best estimates were made from the surveillance weld (even though the heat numbers did not match) and from generic data for welds made in that time period. As a last resort, a value of 0.35% copper was used, that being the value which gave the upper limit or bounding line for all data in Regulatory Guide 1.99 Revision 1 (Ref. E.5) as described below.

E.3.2 Regulatory Guide 1.99 Revision 1, Bounding Curves

Since publication in April 1977, Regulatory Guide 1.99 Rev. 1 contains the procedure recommended by the NRC to obtain ΔRT_{NDT}, the "adjustment of reference temperature" as a function of chemistry and neutron fluence. Copper was the dominant residual element in the chemistry term (the other was phosphorus) as can be seen at the top of Figure E-1. The exponent on the fluence term is 0.5, but there is a cut-off or upper limit line for which the exponent is 0.194 for high copper content and fluence exceeding 6×10^{18} n/cm^2 (E>1 MeV).

Criticism leveled at Regulatory Guide 1.99 became more insistent when the PTS issue made it necessary to look hard at all sources of conservatism. It was said that (a) the curves were too conservative at high fluences, especially for low-nickel materials, and (b) the phosphorus term was not supported by recent studies such as the MPC report (Ref. E.6) described below and should be dropped. Nevertheless, Regulatory Guide 1.99 was used for high-nickel materials by all 3 vendors in the reports that were concurrent with the utilities' 150 day reports. The high-nickel materials are ASTM A 533 plates, A 508 forgings, and welds of comparable chemistry, for which the nickel content is generally between 0.5 and 1.0 percent. The low-nickel materials are ASTM A 302 plates and welds of comparable chemistry, which generally have less than 0.25 percent nickel as a residual element. A relatively small number of older vessels have low-nickel material.

E.3.3 Guthrie Trend Curves

Evidence has been accumulating for several years that the low-nickel materials are less sensitive to neutron radiation. When the PWR surveillance data base was analyzed by the NRC in October 1981, the difference between high and low nickel content material was apparent. Westinghouse and CE reported similar findings and presented empirical equations for the low-nickel material. (B&W have no plants with low-nickel materials in the reactor vessel.) The PWR surveillance data have now been fitted by a multiple regression analysis technique. The work was done at HEDL by George Guthrie, whose name is attached to the new trend curves (Ref. E.7). The Guthrie mean curve is as follows:

$$RT_{NDT} = [-10 + 470\ Cu + 350\ Cu\ Ni]\ [f/101^9]^{0.27}$$

WRT_{NDT} = adjustment of reference temperature, degrees F
Cu = weight percent copper
Ni = weight percent nickel
f = fluence, n/cm^2 ($E > 1$ MeV)

The use of a copper-nickel product term reflects the advice of J. R. Hawthorne (Ref. E.8) of the Naval Research Laboratory to the effect that nickel seems to enhance the effect of copper, but nickel does not cause increased embrittlement in the absence of copper. The product term is also consistent with work reported by Varsik and Byrne (Ref. E.9) in which their "chemistry factor" was the product of copper and a quantity, nickel plus other elements.

Figures E-2, E-3, and E-4 show how the Guthrie formula fits the PWR surveillance data. The residual value (predicted minus measured) for each line of data is plotted against fluence, copper content, and nickel content to give a graphical check on the effectiveness of the multiple regression analysis.

E.3.4 Guthrie Upper Bound Trend Curves

The standard deviation for the data analysis described in paragraph E.3.3 was 24 degrees F. From inspection of Figure E-2, it appears that a constant 2-sigma upper bound is satisfactory over the fluence range of interest.

E.3.5 Comparison with MPC Curves

As further support for the Guthrie mean curve, Figure E-5 gives a comparison of the Guthrie mean curve for representative copper and nickel contents with a mean curve developed by the Metal Properties Council for ASTM Committee E-10 on Nuclear Technology and Applications (Ref. E.6). The latter is being ballotted as an ASTM Standard. The MPC data base contains all of the test reactor and surveillance data that fit the criteria for material form and irradiation temperature that were available in November, 1977. There is reasonably good agreement between the MPC trend curves and the Guthrie curves, considering that the MPC curves were for a range of nickel content, but were without a nickel term in the equation.

The MPC trend curve did not contain a phosphorus term, because in the regression analysis the addition of a phosphorus term did not produce any significant decrease in the residual variance. In a further study of this finding, the MPC Task Group found a statistically significant relationship of phosphorus content to copper content, i.e., high phosphorus was found with high copper. Thus, their combined effects were represented in the trend curve formulation by a copper term alone.

E.3.6 Comparison with Vendor's Curves

Westinghouse and Combustion Engineering drew bounding curves for low-nickel material. Figure E-6 gives a comparison of the Guthrie mean plus 2-sigma curves for 0.15% nickel material with the low-nickel trend curves presented by Westinghouse and CE. The latter lie below the Guthrie curves over most of the range of fluence.

E.4 Screening Value of RT_{NDT}

The Working Group on RT_{NDT} (Ref. E.10) agreed that the value of RT_{NDT} to be used in screening plants should be calculated as the sum of 3 quantities: the mean value of initial RT_{NDT} ($RT_{NDT\ o}$), plus the mean value of ΔRT_{NDT} at the inside surface of the vessel, plus twice the square root of the sum of the squares of the standard deviation on each, i.e., $2\sqrt{\sigma_o^2 + \sigma\Delta^2}$.

E.4.1 Uncertainties

Uncertainties in the screening value of RT_{NDT} arise from several sources. Those associated with the estimate of initial RT_{NDT} were discussed in Appendix E.2. For ΔRT_{NDT}, there is the scatter about the trend curve (shown in Figures E-2, E-3 and E-4) which is made up of the uncertainty in response of material to radiation, plus errors in the copper and fluence values in the data base and errors in the Charpy shift measurement itself. In addition, there is uncertainty in the copper content of the critical weld in the vessel. Because copper was introduced as a plating on the weld wire, and plating thickness was not controlled, variation in copper content through the vessel wall and along the length of the weld is expected to be considerable. From a number of measurements for certain weld wire heat numbers, one standard deviation is expected to be about 0.03 percent copper, typically. This is equivalent to 15 degrees F in the plants with higher fluences.

Nevertheless, the copper contents used in calculating RT_{NDT} for plants were best-estimate values. They were not mean plus 2 sigma values. This is one reason why the Working Group on RT_{NDT} felt that the screening values should have the 2 sigma measure of error added to the mean.

E.4.2 Alternative Calculation of RT_{NDT}

For high values of copper and nickel contents, the method described above gives values higher than those predicted by that part of the Upper Limit of R.G. 1.99, given by the equation:

$$\Delta RT_{NDT} = 283\ (f/10^{19})^{0.194}$$

Experience has shown that the latter bounds the available data. Therefore, the screening value of RT_{NDT} is taken to be the lower of two quantities:

$$RT_{NDT} = RT_{NDT\ o} + \text{Guthrie Mean } \Delta RT_{NDT} + 2\sqrt{\sigma_o^2 + \sigma_W^2}$$

or

$$RT_{NDT} = RT_{NDT\ o} + 283\ (f/10^{19})^{0.194} + 2\ \sigma_o$$

as illustrated schematically in Figure E-7.

The 2-sigma term in the second equation does not include the error in ΔRT_{NDT} because the term for ΔRT_{NDT} is an upper-bound equation.

The Upper Limit line of R.G. 1.99 actually consists of two branches, the one described above, for fluences above 6×10^{18}, and a lower branch that has an exponent of 0.5. The latter was not used, because it does not bound all of the observed data in that fluence range. Thus, for the purpose of this screening criterion, the alterative equation,

$$\Delta RT_{NDT} = 283\ (f/10^{19})^{0.194}$$

is used at fluences below 6×10^{18} as well as for higher fluences.

REFERENCES

E.1. Summary Report on Reactor Vessel Integrity for Westinghouse Operating Plants by T. A. Meyer, December, 1981, WCAP 10019.

E.2. Evaluation of Pressurized Thermal Shock Effects Due to Small Break Loca's With Loss of Feedwater for the Combustion Engineering NSS, CEN-189 Prepared for the C-E Owner's Group by Combustion Engineering, Inc., December 1981.

E.3. PNL Technical Review of Pressurized Thermal Shock Issues Draft Report, June 1982.

E.4. Methods of Compliance with Fracture Toughness and Operational Requirements of 10 CFR 50, Appendix G, B&W-100 46A, Rev. 1, July 1977, by H. S. Palme, H. W. Behnke, and W. S. Keysworth, Babcock and Wilcox.

E.5. Effects of Residual Elements on Predicted Radiation Damage to Reactor Vessel Materials, Regulatory Guide 1.99, Revision 1, April 1977.

E.6. Prediction of the Shift in The Brittle-Ductile Transition Temperature of LWR Pressure Vessel Materials, Edited by J. J. Koziol, Prepared by a Task Group of Subcommittee 6 of the Metal Properties Council, April 6, 1981 as a Report to ASTM Committee E10.01. To be published by ASTM.

E.7 LWR Pressure Vessel Irradiation Surveillance Dosimetry Quarterly Progress Report, Jan.-Mar. 1982, HEDL-TME 82-18. To be published

E.8. J. R. Hawthorne, "Status of Knowledge of Radiation Embrittlement in USA Reactor Pressure Vessel Steels," NUREG/CR-2511, NRL Memo Report 4737, Feb. 1982.

E.9 J. D. Varsik and S. T. Byrne, "An Empirical Evaluation of the Irradiation Sensitivity of Reactor Pressure Vessel Materials," Effects of Radiation on Structural Material, ASTM STP 683, 1979, pp 252-266.

E.10. Findings of the Ad Hoc Working Group on Selection of RT_{NDT}, Memorandum M. Vagins to S. Hanauer, Aug. 30, 1982.

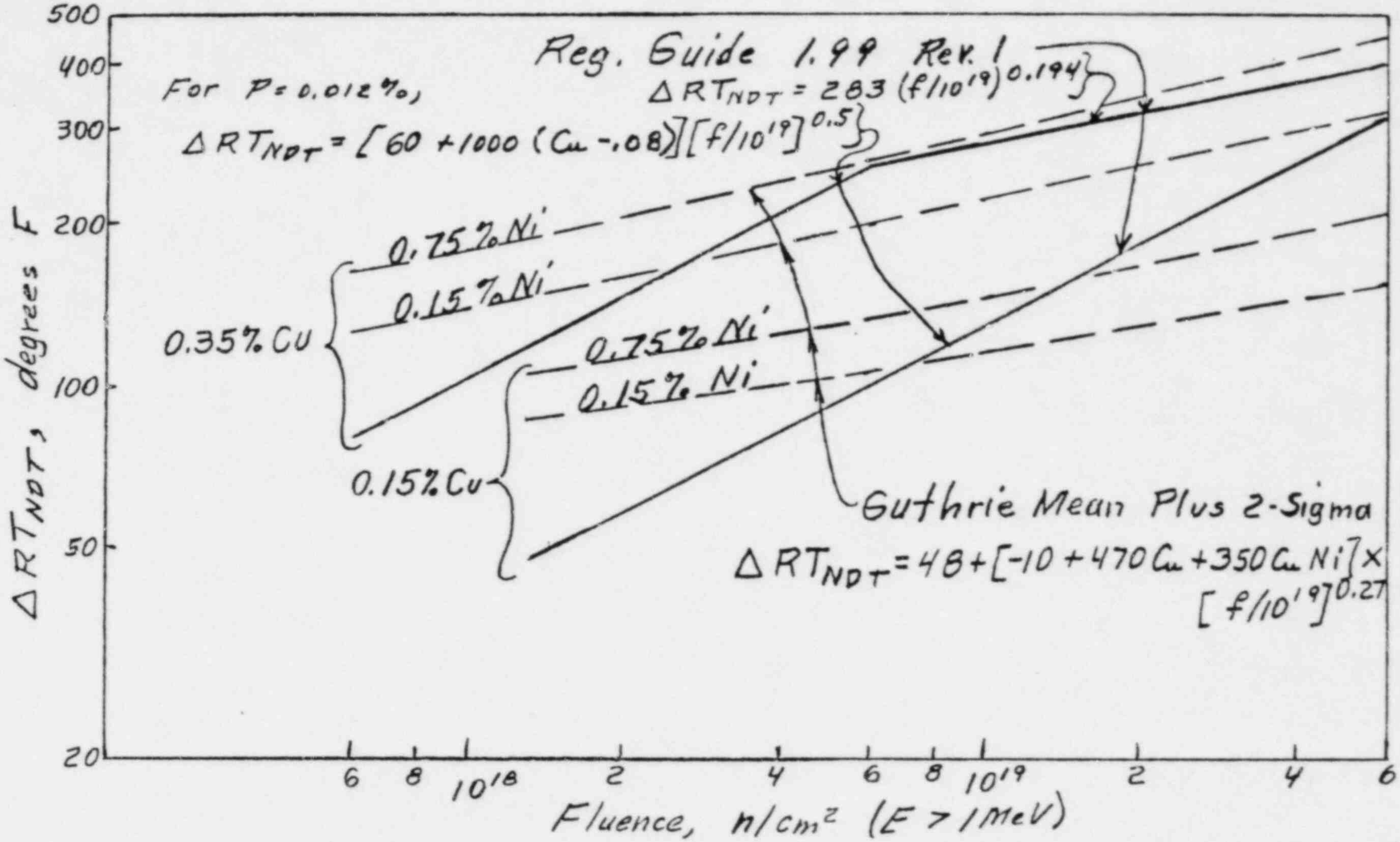


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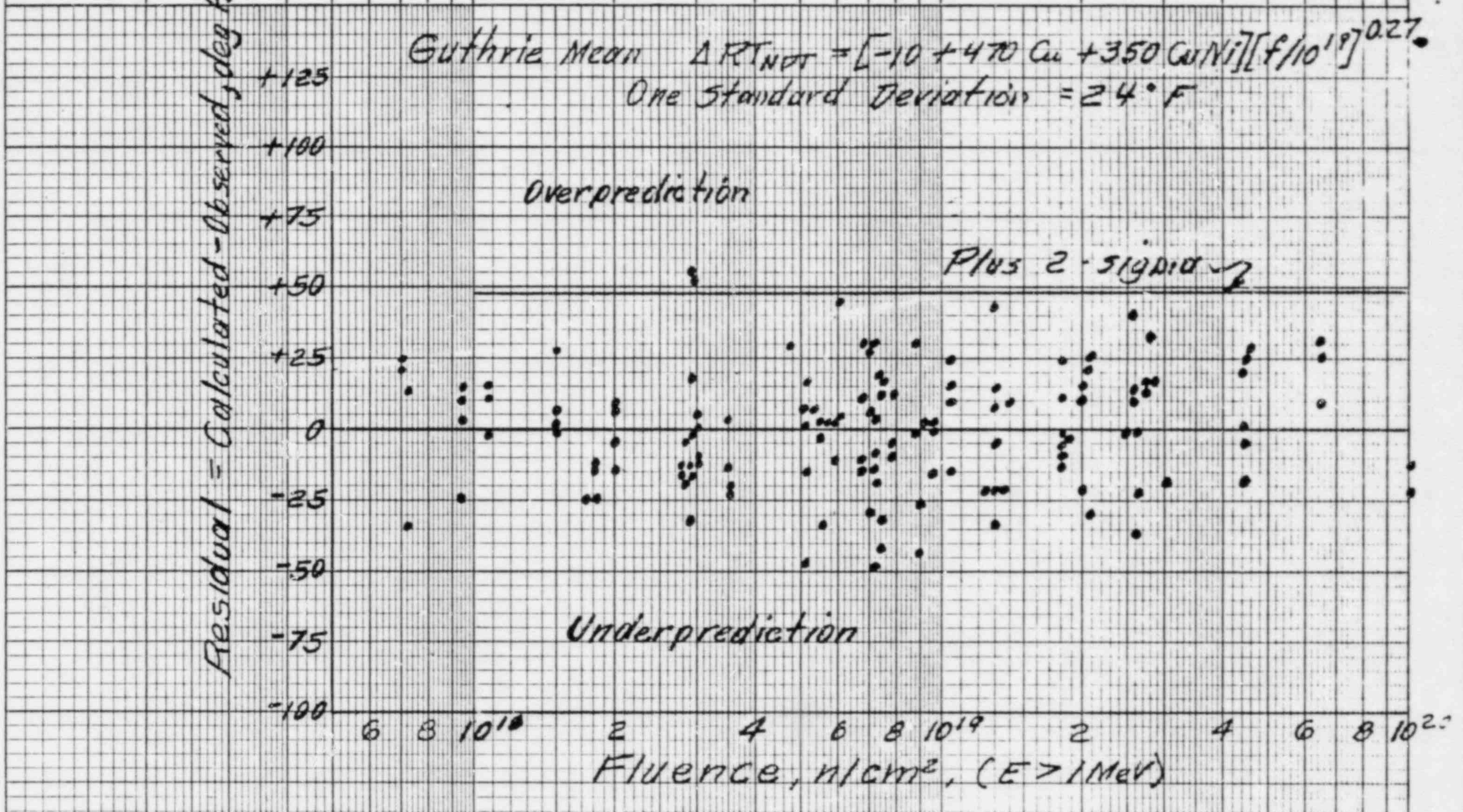
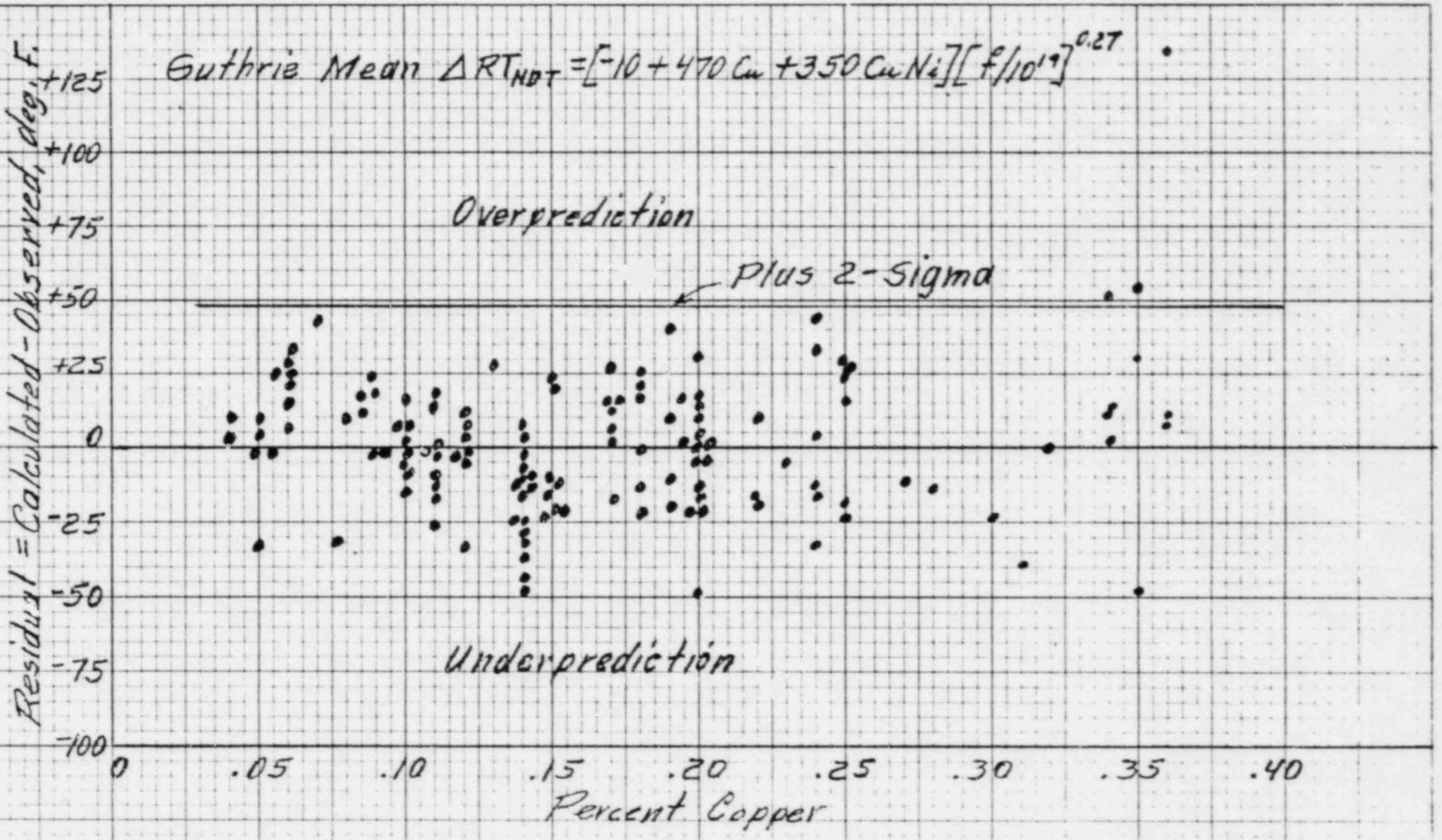
Figure E1 Comparison of Reg. Guide 1.99 Rev. 1 and Guthrie Mean Plus 2-Sigma Trend Curves



~~Fig. E-2~~ Analysis of the Fit of the Guthrie Formula to the PWR Surveillance Data Base.
Effect of Fluence

Fig. E-2

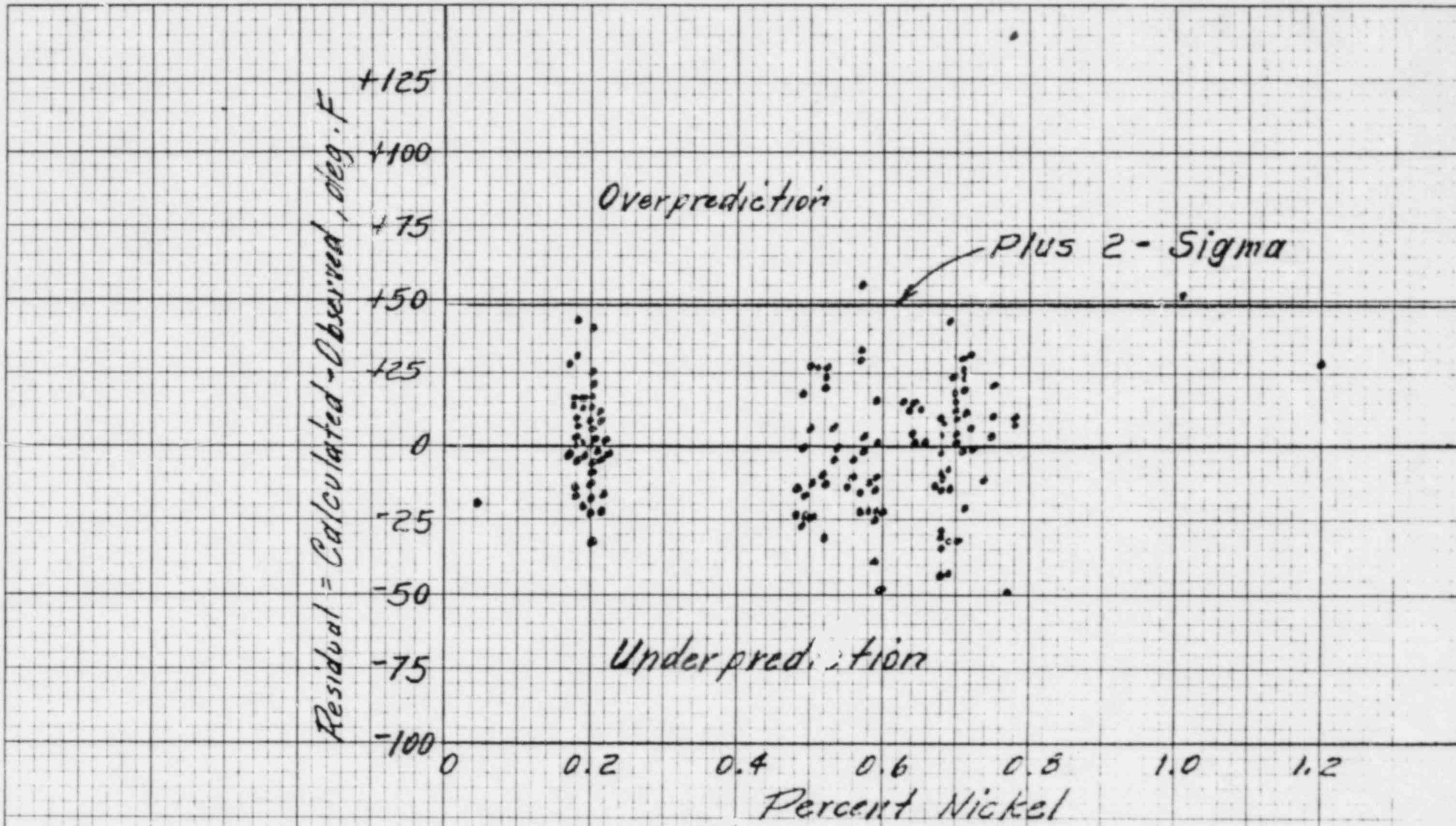
~~E-8~~ 8-17



~~Figure 8.3~~ Analysis of the Fit of the Guthrie Formula to the PWR Surveillance Data Base Effect of Copper Content

Fig E-3

8-13 ~~6-8~~



~~Figure 8-4~~ Analysis of the Fit of the Guthrie Formula to the PWR Surveillance Data Base Effect of Nickel Content

Fig. 8-1

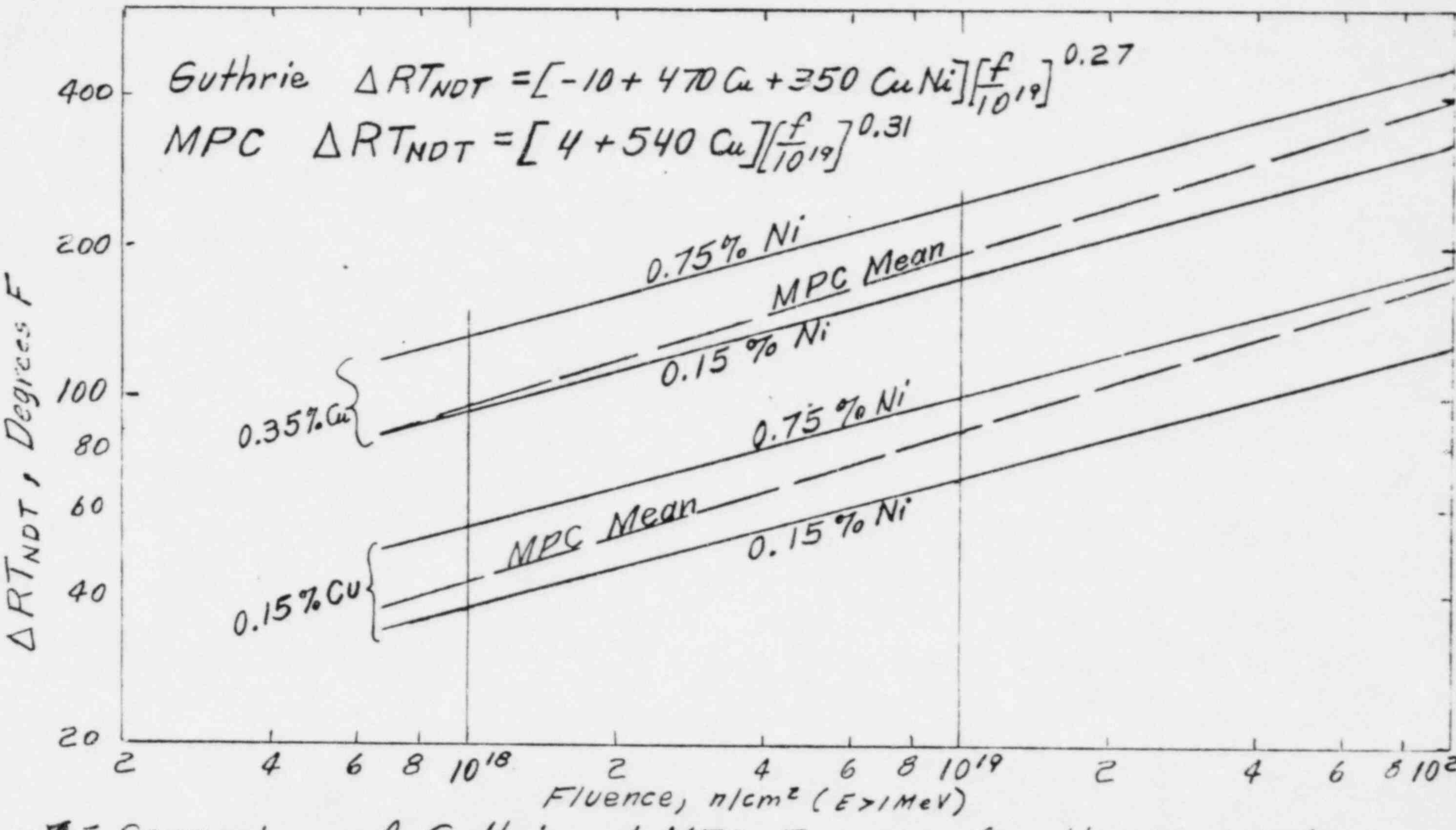


Figure B.5 Comparison of Guthrie and MPC Formulas for the Mean Values of ΔRT_{NDT} for Representative Copper and Nickel Contents

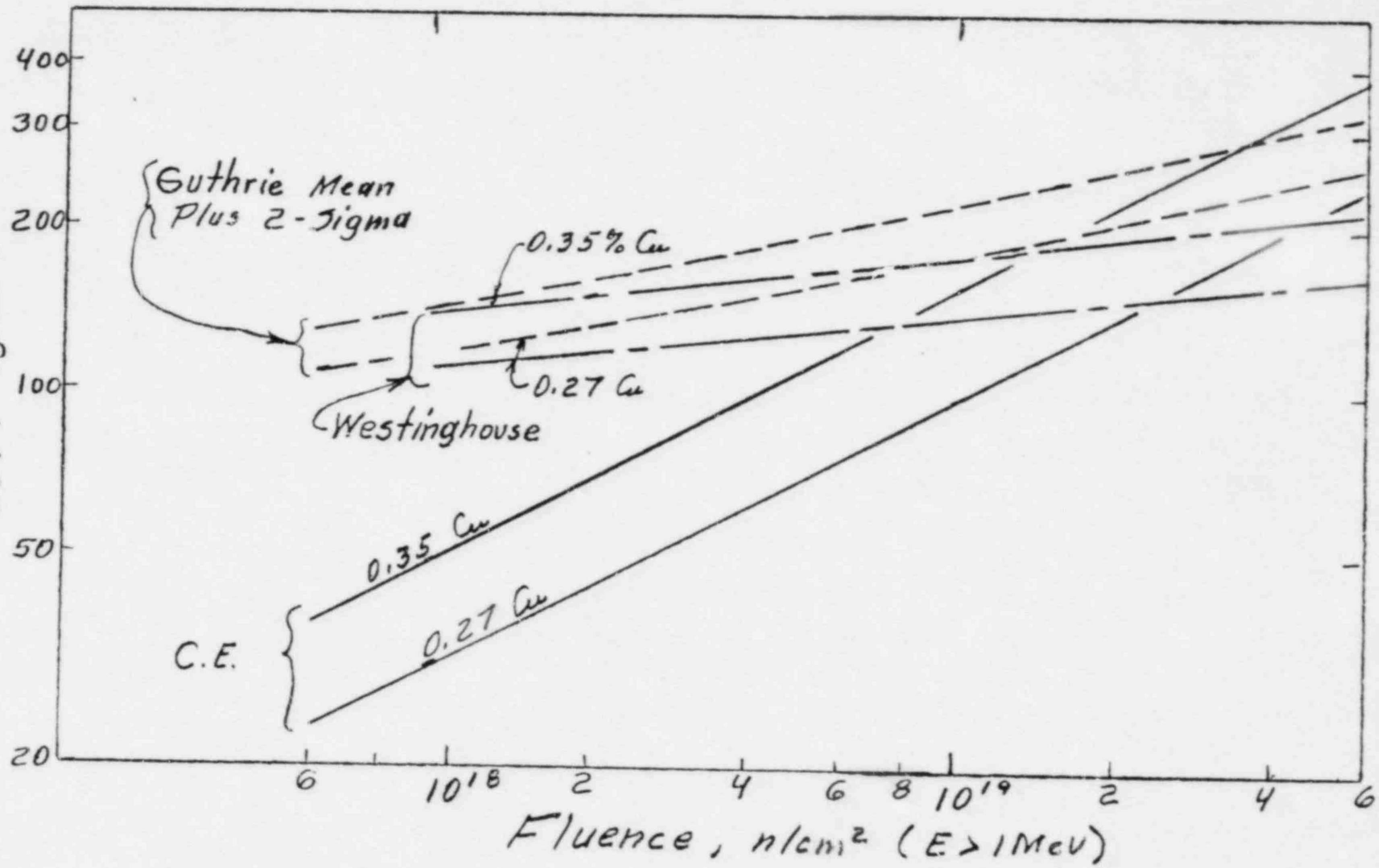


Figure E.6 Comparison of C.E., Westinghouse and Guthrie Trend Curves for Low-Nickel Material

12-8 ~~8-13~~

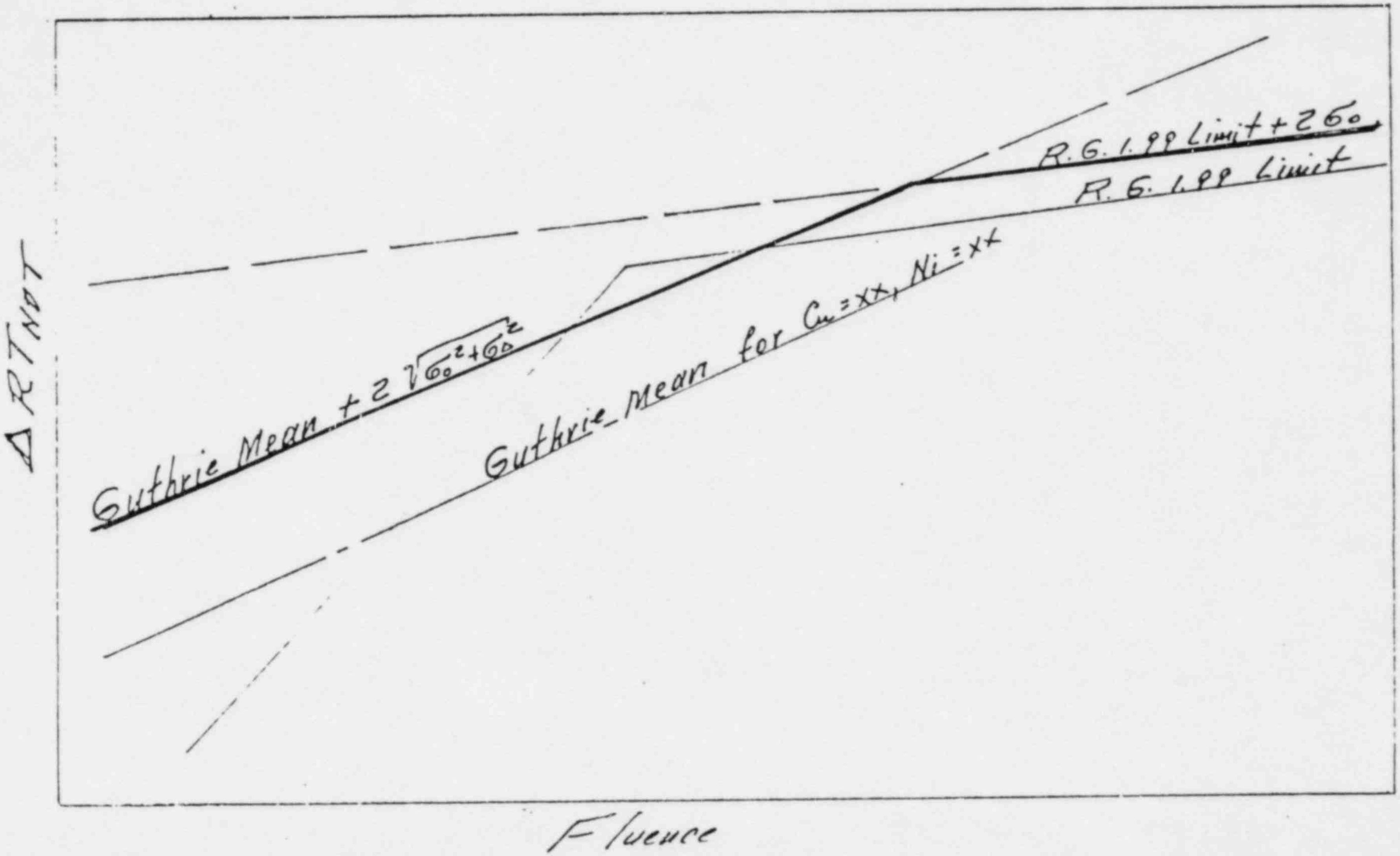

R.G. 1.99 Limit + 2σ₀
R.G. 1.99 Limit
Guthrie Mean + 2 $\sqrt{\sigma_0^2+\sigma_0^2}$
Guthrie Mean for Cu = x%, Ni = x%
ΔRTNDT
Fluence

Figure E-7

APPENDIX F

PRESSURE VESSEL FAST NEUTRON FLUENCE UNCERTAINTY

F.1 Introduction

The following discussion deals with the components of the staff's fast fluence ($\geq$1 MeV) predictive calculational uncertainty.

There are two major sources of uncertainty in fast fluence computations, i.e., (a) the uncertainty which results from the measured values of the fluence used in benchmarking the computer codes, and (b) computational, which originates from uncertainties of input quantities to the code.

F.2 Benchmarking Uncertainty

The prediction of the calculation is benchmarked to measured values of carefully performed experiments. The benchmarking process has been instrumental in recent improvements of the uncertainty as shown in Figure F-1. It can be seen that in the early years of commercial nuclear power the predictive uncertainty was very large. Figure F-1 represents the FSAR predicted values of the fluence and their comparison to a posterior measured value with the surveillance capsule. Measured values from the surveillance capsules and the Pool Critical Assembly improved the predictive capability in the 1970s and is shown in 1980-81 when surveillance capsules were removed. The staff has a technical assistance program at BNL to benchmark the neutron transport code DOT 3.5 and verify the fluence values in the eight pressure vessels which have been thought to have marginal toughness. At this time the benchmarking is nearly complete.

The benchmarking includes data from the following:

(i) The Pool Critical Assembly pressure vessel dosimetry benchmark experiment (Ref. F.1).

In this experiment, the neutron spectra through the various regions from the core to the pressure vessel were measured. The limiting accuracy of the neutron exposure parameters is in the range of ±5 to ±15 percent (1σ) (Ref. F.2).

(ii) The ANO-1 surveillance capsule and reactor cavity flux measurements (Refs. F.3, F.4, F.5, and F.6).

EPRI-sponsored measurements in the reactor cavity provide flux values to an estimated accuracy of ±15 percent (undesignated distribution). Surveillance capsule measurements are being used to adjust the fluence calculated on the inside of the pressure vessel.

(iii) Fort Calhoun surveillance capsule.

(iv) Maine Yankee surveillance capsule.

Figure F-2 shows a typical configuration of a surveillance capsule. The overall length corresponds to that of a fuel assembly and contains an upper, middle, and lower tensile monitor compartments. Tensile specimens are housed in this section along with radiation monitors (Figure F-4). Charpy impact specimens are housed in separate compartments (Figure F-3). Typical locations of surveillance capsules are shown in Figure F-5.

The causes of uncertainty in dosimetry measurements are related to reaction rate cross-sections, the photofission correction, counting calibration, flux-time history, etc. The overall benchmarking uncertainty is ±15 percent (1σ).

F.3 Computational Uncertainty

Computational uncertainty results from uncertainties in cross-section data (inelastic scattering of iron is a particular source of error), modeling, numerical methods, source representation, geometry, etc., which are inputs to the DOT 3.5 code. The DOT series of codes are two-dimensional neutron transport codes based on finite differencing with anisotropic scattering in (x, y), (r, θ), or (r, z) geometries. The DOT 3.5 version is operational at BNL (Ref. F.7).

In order to evaluate calculational uncertainties and provide an additional independent assessment of the uncertainty, a direct parametric analysis is being performed. In this analysis major uncertainty components (e.g., source representation, geometry, cross-section, etc.) have been identified and are being quantified. DOT sensitivity calculations are being performed to propagate these uncertainties and determine their effect on vessel fluence and ΔRT_{NDT} (Ref. F.8). The expected uncertainty is ±15 percent (1σ).

We estimate the overall predictive uncertainty to be ±20 percent (1σ) comparable to ±15 to ±20 percent recently claimed by the vendors (Refs. F.9 and F.10).

The above is illustrated in diagrammatic form in Figure F-6 which illustrates the overall uncertainty, its components, and the sources of the experimental uncertainty.

REFERENCES

F.1 W. N. McElroy, et al., "Surveillance Dosimetry of Operating Power Plants," HEDL-SA-2546, NRC Ninth Water Reactor Safety Information Meeting, NBS, Washington, D.C., October 1981.

F.2. W. N. McElroy, Editor, "LWR Pressure Vessel Surveillance Dosimetry Improvement Program," NUREG/CR-1861, July 1981.

F.3. N. Tsoulfanidis, et al., "Neutron Fluxes in the Core and the Cavity of a PWR," EPRI RP-827-4, draft, February 1981.

F.4. A. L. Lowe, Jr., et al., "Analysis of Capsule ANI-E from Arkansas Power and Light Company, Arkansas Nuclear One Unit 1, "BAW-1440, Lynchburg, Virginia 24505, April 1977.

F.5. R. Mearker, et al., "Development and Demonstration of an Advanced Methodology for LWR Dosimetry Applications," EPRI (RP-1399-1) confidential, draft.

E.6. J. Grundl, et al., "NRC-EPRI Studies of Pressure-Vessel-Cavity Neutron Fields," NRC Ninth Water Reactor Safety Information Meeting, NBS, Washington, D.C., October 1981.

E.7. DOT-3.5, "Two Dimensional Discrete Ordinates Radiation Transport Code," RSIC Computer Code Collection CCC-276, 1976.

E.8. J. Carew, BNL, Personal Communication, December 7, 1981.

E.9. "Letter Report on Reactor Vessel Brittle Facture Concerns in B&W Operating Plants," Document ID 77-1125756, May 15, 1981.

E.10. "Thermal Shock to Reactor Pressure Vessel," letter from R. W. Jurgenson (Chairman, Westinghouse Users Group) to D. G. Eisenhut, May 14, 1981.

REFERENCES

F.1 W. N. McElroy, et al., "Surveillance Dosimetry of Operating Power Plants," HEDL-SA-2546, NRC Ninth Water Reactor Safety Information Meeting, NBS, Washington, D.C., October 1981.

F.2. W. N. McElroy, Editor, "LWR Pressure Vessel Surveillance Dosimetry Improvement Program," NUREG/CR-1861, July 1981.

F.3. N. Tsoulfanidis, et al., "Neutron Fluxes in the Core and the Cavity of a PWR," EPRI RP-827-4, draft, February 1981.

F.4. A. L. Lowe, Jr., et al., "Analysis of Capsule ANI-E from Arkansas Power and Light Company, Arkansas Nuclear One Unit 1, "BAW-1440, Lynchburg, Virginia 24505, April 1977.

F.5. R. Mearker, et al., "Development and Demonstration of an Advanced Methodology for LWR Dosimetry Applications," EPRI (RP-1399-1) confidential, draft.

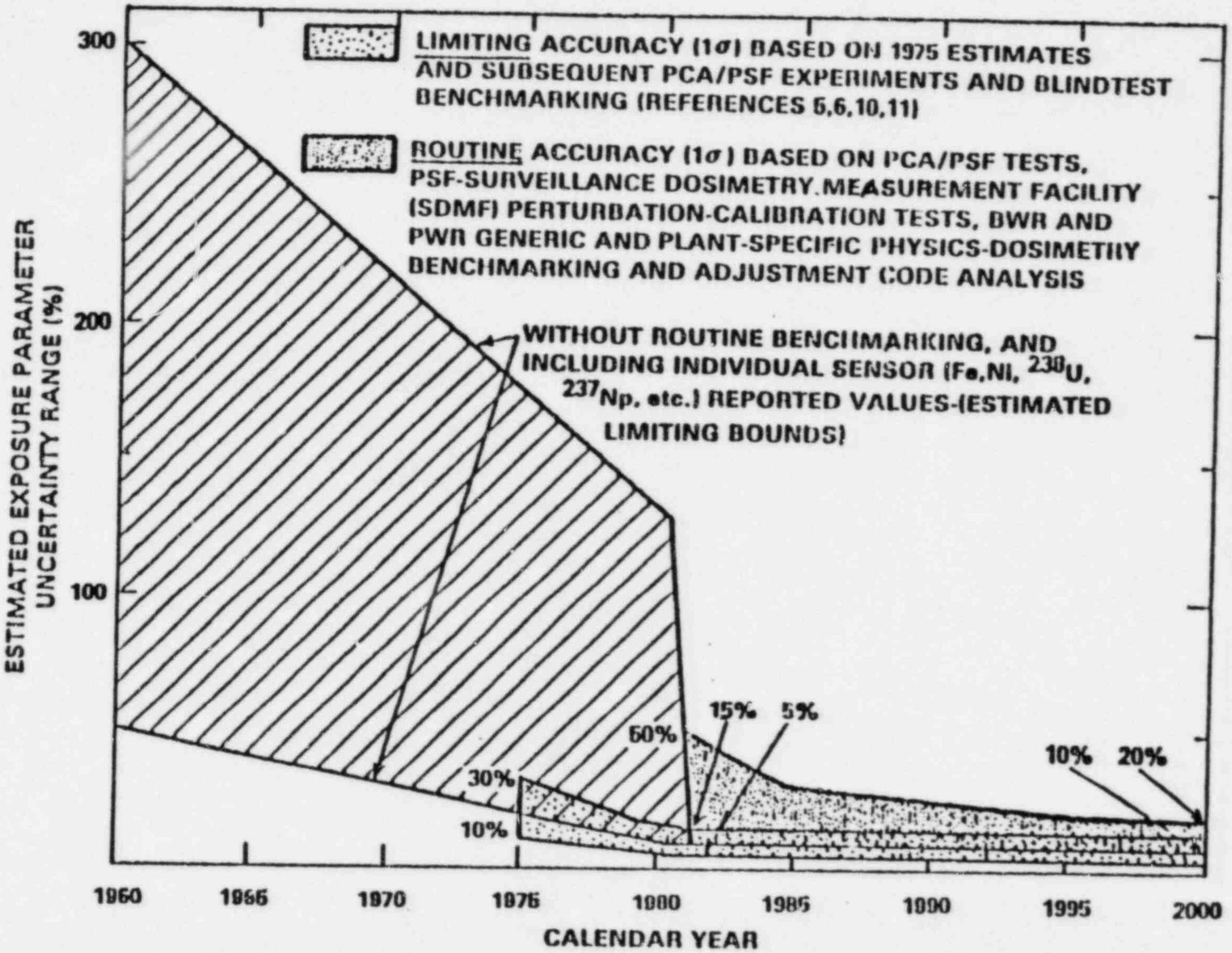
F.6. J. Grundl, et al., "NRC-EPRI Studies of Pressure-Vessel-Cavity Neutron Fields," NRC Ninth Water Reactor Safety Information Meeting, NBS, Washington, D.C., October 1981.

F.7. DOT-3.5, "Two Dimensional Discrete Ordinates Radiation Transport Code," RSIC Computer Code Collection CCC-276, 1976.

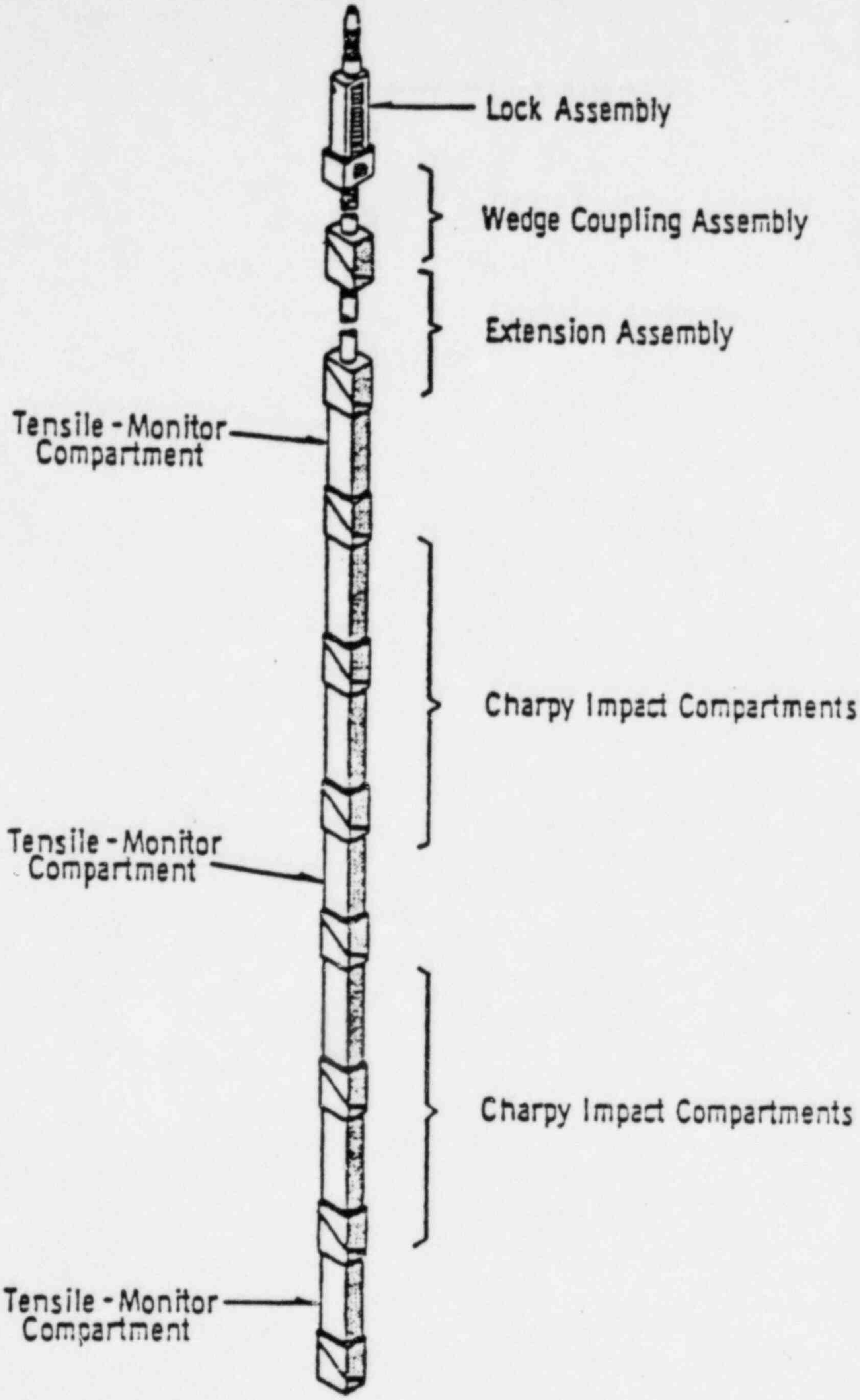
F.8. J. Carew, BNL, Personal Communication, December 7, 1981.

F.9. "Letter Report on Reactor Vessel Brittle Facture Concerns in B&W Operating Plants," Document ID 77-1125756, May 15, 1981.

F.10. "Thermal Shock to Reactor Pressure Vessel," letter from R. W. Jurgenson (Chairman, Westinghouse Users Group) to D. G. Eisenhut, May 14, 1981.

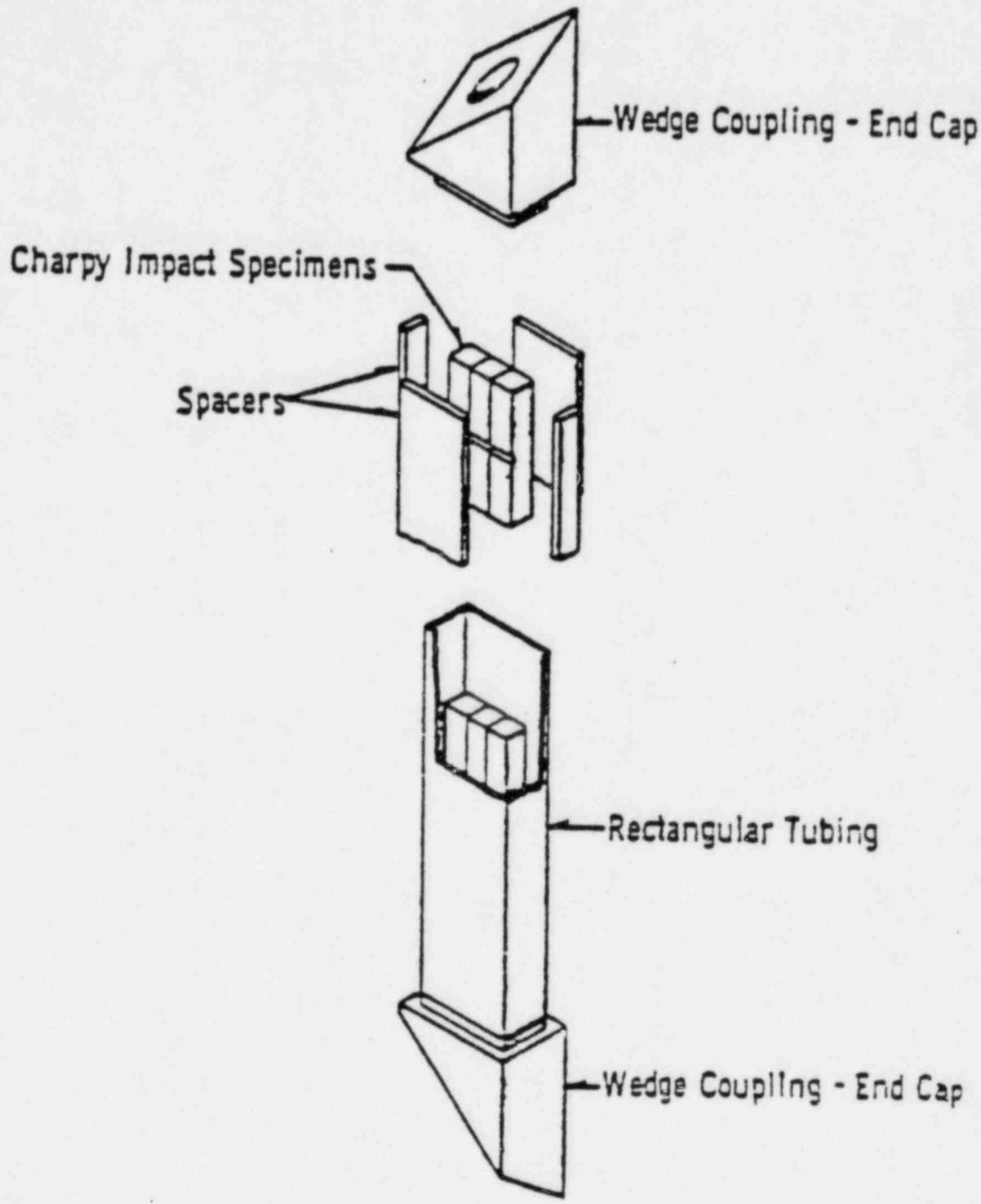


Estimated Exposure Parameter Uncertainties Obtained from FSAR and Surveillance Capsule Reports.



OMAHA PUBLIC POWER DISTRICT Fort Calhoun Station Unit No. 1	SURVEILLANCE CAPSULE ASSEMBLY

Fig. F-2



OMAHA PUBLIC POWER DISTRICT Fort Calhoun Station Unit No. 1	CHARPY IMPACT COMPARTMENT ASSEMBLY

Fig. F-3 ~~2~~

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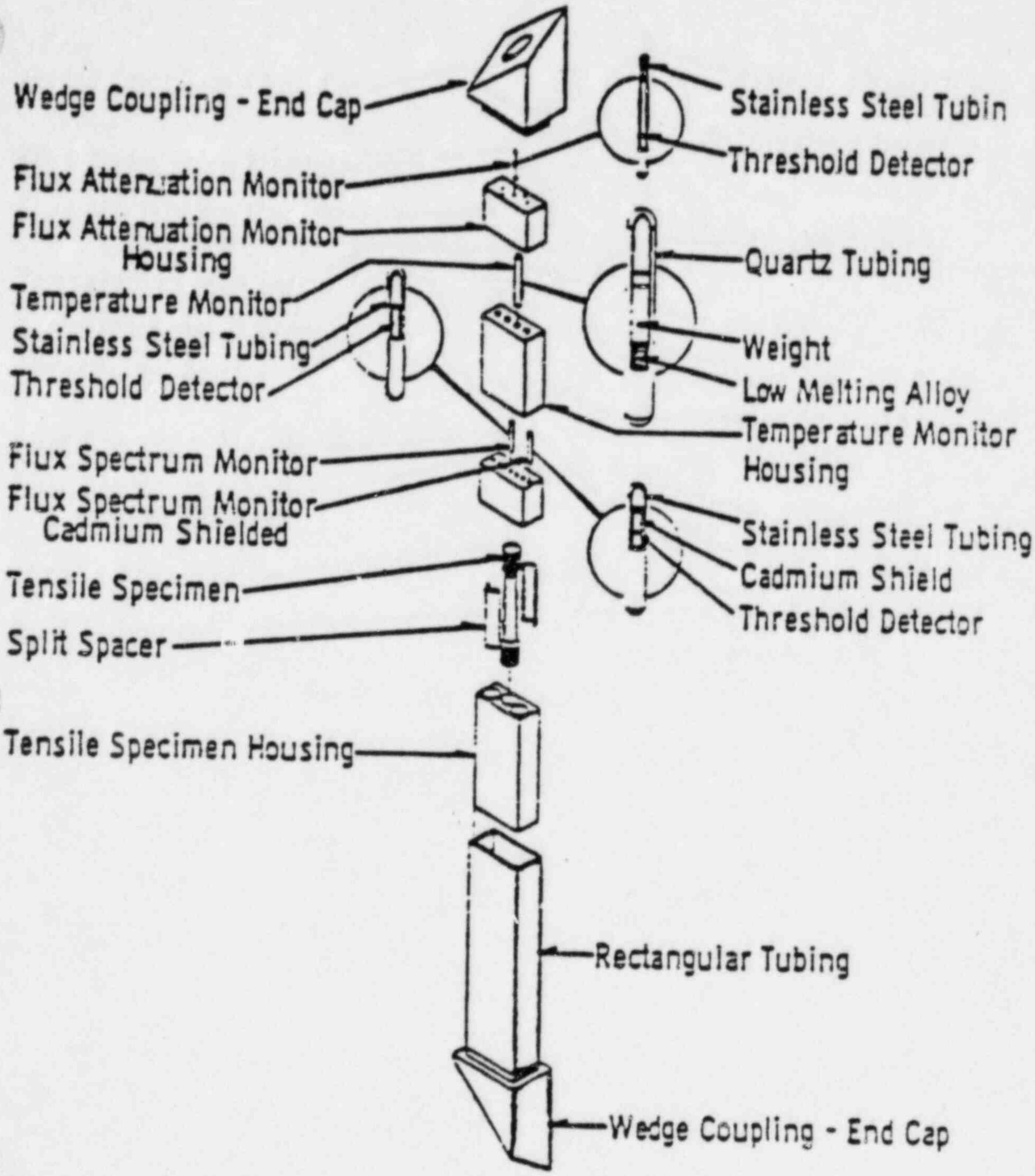
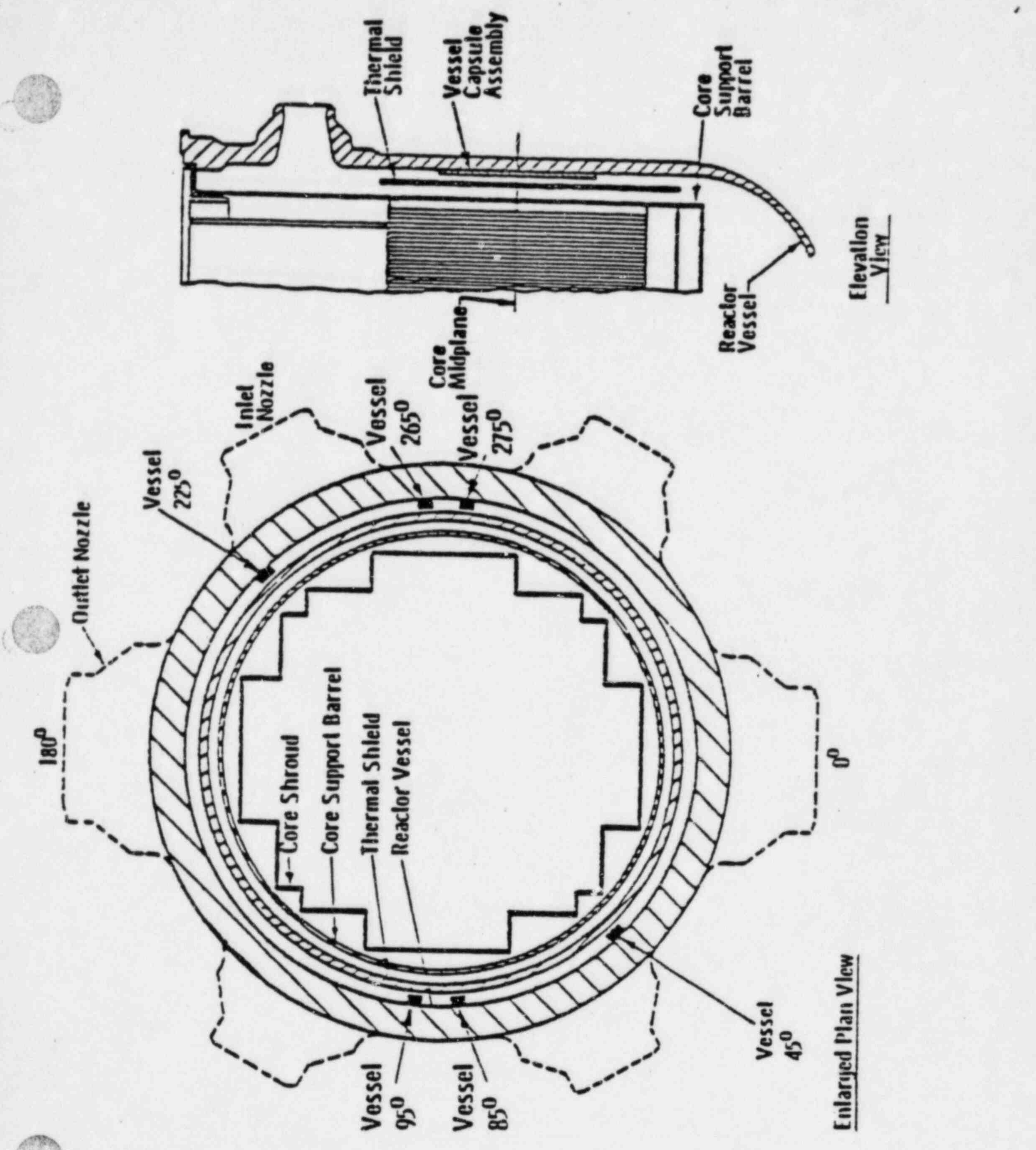


OMAHA C POWER DISTRICT Fort Calhoun Station Unit No. 1	TENSILE-MONITOR COMPARTMENT ASSEMBLY

F-4
Fig. A



OMAHA PUBLIC POWER DISTRICT Fort Calhoun Station Unit No. 1	LOCATION OF SURVEILLANCE CAPSULE ASSEMBLIES

Fig. F.5

F.8

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FAST FLUENCE UNCERTAINTY

± 20% (1σ) Predictive Uncertainty

Benchmarking (experimental)
± 15% (1σ)

Calculational Uncertainty
± 10-15% (1σ)

Benchmarking

Pool Critical Assembly

ANO-1: Surveillance Capsure
Reactor Cavity

Ft. Calhoun: Surveillance
Capsule

Maine Yankee: Surveillance
Capsule

Calculational

Sensitivity analysis to be performed for major uncertainty components

F.6
Fig. 8

APPENDIX H

REACTOR PRESSURE VESSEL FAILURE PROBABILITY STUDY

Reactor pressure vessels (RPV) in nuclear power plants have traditionally been considered extremely reliable structural components. Indeed, studies completed in the United States and Europe have concluded that the disruptive failure rate (loss of the pressure retaining boundary) for nuclear pressure vessels is less than 10^{-6} at a 99% confidence level for RPVs designed, fabricated, inspected, and operated in accordance with the Boiler and Pressure Vessel Code of the American Society of Mechanical Engineers. However, recent results from surveillance and research programs and operating experience suggest that the issue of RPV failure probability should be reassessed. The renewed interest in RPV failure probability is due to the observation that thermal hydraulic transients occurring in commercially operating nuclear power plants are subjecting RPVs to unanticipated loadings which could contribute significantly to the failure probability of RPVs. In addition, operating experience and research programs over the past few years have provided additional information that more clearly defines both material property variations in RPVs and the effect of neutron irradiation on the material's resistance to fracture. The objective of this study is to assess the contribution to RPV failure probability of recently observed thermal hyraulic transients using the most recent material property data.

Generally, RPV reliability studies have used either one of two methods to calculate the probability of RPV failure. These methods are (1) the analysis of statistical data from observed non-nuclear pressure vessel failures to infer failure rates for both nuclear and non-nuclear pressure vessels and (2) the use of mathematical models that predict failure rates by analytically generating pressure vessel failures. Mathematical models used in the later technique have been primarily closed form analyses. In this effort, Monte Carlo simulation techniques have been used because of the ability to consider a greater number of significant random variables and to perform a wide range of sensitivity studies.

The results of extensive sensitivity studies which have been conducted are extremely important because they quantify the effect of uncertainties in the input parameters, thereby providing an estimate of the accuracy of the calculated failure probabilities, and they identify the significant variables and variable interactions. The results are best applied in a relative sense for use in decision making, and extreme caution must be exercised in applying the results in an absolute sense.

Section H.1 of this report describes the reactor pressure vessel considered in this study, Section H.2 describes the fracture mechanics techniques and simulation model used to calculate RPV failure probabilities; Section H.3 presents results of a reference case and sensitivity analysis performed using the simulation code; and Section H.4 presents a discussion and conclusions of the study.

H.1 Reactor Pressure Vessel Description

The reactor vessel geometry in this study has a 9-inch wall thickness and a 90-inch mean radius. Figure H-1 presents a schematic of how the RPV is fabricated. The failure probability is calculated for one vertical weld in the two beltline shell courses, which have lengths of approximately 72 inches. These dimensions are typical of most operating PWR vessels. Only the welds are considered because they have the greatest propensity for flaws, are most sensitive to radiation damage, and hence, should dominate the failure probability. The reactor vessel is fabricated of carbon steel with stainless steel cladding on the internal surfaces that are in contact with the primary coolant.

H.2 Probabilistic Model

H.2.1 Fracture Mechanics Algorithms

Pressurized thermal shock transients can subject the reactor pressure vessel to an unusual combination of high thermal and pressure stresses that create the potential for fracture of the reactor pressure vessel. Given well defined pressure and temperature-time histories for a pressurized thermal shock transient, heat transfer and stress analyses can be conducted using either closed form or numerical analysis techniques.

In this study closed form solutions have been utilized for the heat transfer and stress analyses. The closed form solutions allow the primary coolant temperature time history to be expressed as either a fourth order polynomial or an exponential function of the form:

$$T = T_o - (T_o - T_f)e^{-\beta t} \qquad \text{(H-1)}$$

where T is the temperature of the primary coolant as a function of time; T_o and T_f are the initial and final primary coolant temperatures, respectively; β is the decay constant that determines the rate of cooldown; and t is time. The pressure time history is represented by a fourth order polynomial. The heat transfer analysis is performed using an effective heat transfer coefficient which takes into account the fluid film heat transfer coefficient and the thermal resistance of the stainless steel cladding. However, the stresses due to the difference in thermal expansion between the stainless steel cladding and the base metal have not yet been included in the probabilistic code. A sensitivity study in Section H.3.2.9 provides an indication of how these stresses might affect the calculated failure probabilities.

The temperature and stress intensity values calculated using the above techniques were found to be in excellent agreement with the temperatures and stress intensity values calculated by the OCA-I code developed at ORNL.

Once the transient temperature and stress states have been calculated for the pressurized thermal shock event, linear-elastic fracture mechanics analysis is used to evaluate RPV integrity. Linear-elastic fracture mechanics (LEFM) is used to determine if a pre-existing flaw will propagate unstably through a material under certain loading and material conditions. The LEFM criteria for unstable fracture is:

$$K_I > K_{Ic} \qquad \text{(H-2)}$$

where K_I is the applied stress intensity factor and K_{Ic} is the critical stress intensity factor. Warm prestressing which can effectively inhibit crack extension even when K_I exceeds K_{Ic} (see Section D.3) was not considered in the analyses with the exception of the sensitivity studies presented in Section H.3.5. Although for many of the transients analyzed, warm prestressing would be effective, these transients were only assumed for convenience in conducting parametric studies. System considerations and operator actions do not ensure that warm prestressing will be effective in every case.

The applied stress intensity factor, K_I, is a function of the stress state; crack depth, a; and flaw and component geometry. The stress state at any time in a pressurized thermal shock transient is defined by the pressure and temperature-time histories. The component geometry of interest in this study is the RPV beltline with an assumed longitudinally oriented flaw. The assumed longitudinal orientation is that expected in longitudinally oriented welds and is the flaw orientation that experiences the maximum stress and K_I in the reactor vessel beltline. Deterministic analyses assume that a flaw of a specific depth exists with certainty. In the probabilistic model developed in this study, the crack depth is treated as a random variable.

The critical stress intensity factor, K_{Ic}, is the material's resistance to unstable fracture. K_{Ic} is a function of the temperature at the crack tip; the material's initial nil-ductility reference temperature, RT_{NDTo}; and the shift in RT_{NDT}, ΔRT_{NDT}. The temperature at any depth in the vessel wall is defined by the heat transfer analysis of the pressurized thermal shock transient.

RT_{NDTo} is a material property determined by a destructive material testing procedure and is a measure of the temperature at which the material begins a transition from a "brittle" to ductile fracture mode. Determination of RT_{NDTo} is subject to material variability and measurement errors. Furthermore, estimates of the RT_{NDTo} for a specific plant often must be made from a generic data base not totally representative of the specific material of interest. Therefore RT_{NDTo} is treated as a random variable in the probabilistic model.

The shift in RT_{NDT} is a result of neutron irradiation. As the vessel beltline fluence increases, the RT_{NDT} of the material becomes higher. This means that in order to exhibit the same resistance to fracture, K_{Ic}, the material must be at a higher temperature. The attenuation of fluence through the RPV wall for the results presented in this study was represented by the following relation

$$F(a) = F_{ID}\, e^{-.33a} \tag{H-3}$$

where a is the depth in inches into the vessel wall and F_{ID} is the fluence (> 1 MEV) in neutrons/cm^2 at the surface of the RPV wall. More recent studies based on the concept of displacement per atom, dpa, consider a wider spectrum of neutron energies and suggest that the exponential decay constant should be smaller to more accurately predict radiation damage through the RPV wall. Fluence on the inside surface of the RPV wall varies with location in the RPV beltline due to the core design and power profile. In addition, there are relatively large uncertainties in calculating fluences. Thus, fluence has been considered a random variable in this study.

In the probabilistic analyses, the mean shift in RT_{NDT} has been represented by the following function:

$$\Delta RT_{NDT} = [-4.83 + 476 \cdot Cu + 267 \cdot Cu \cdot Ni]\,[F/10^{19}]^{0.218} \tag{H-4}$$

where ΔRT_{NDT} is the mean shift in RT_{NDT}, Cu is the copper content in weight percent, Ni is the nickel content in weight percent, and F is the fluence in neutrons (> 1 MEV)/cm^2. This equation was developed at HEDL through regression analysis of surveillance and research program results. Copper and nickel contents vary throughout the RPV material, and uncertainties exist with the values specified for plant specific welds. Hence copper and nickel contents should be treated as random variables. Copper content was treated as a random variable in this study. However, the effect of nickel has just recently been recognized; and hence, nickel was not considered as a random variable in the original development of the code. Future versions of the code wil include nickel as a random variable. The results presented here were generated assuming a constant nickel content of 0.65%.

The surveillance and research program data on ΔRT_{NDT} as a function of fluence exhibit significant variability as illustrated in Figure H-2. However, it is believed that much of the variability is due to variability and uncertainty in the measured fluences and copper contents in the data base. Therefore, it seems inappropriate to consider this variability twice and for the results presented in this study the mean trendline for ΔRT_{NDT} versus fluence specified in equation (H-4) was used. A proposed sensitivity study to be conducted in the future is to compare the results of this study with results generated by using mean copper contents and fluences and treating ΔRT_{NDT} as a random variable. However, for this study it was desirable to be able to conduct sensitivity studies on copper content and fluence; hence, these parameters were treated as random variables.

Once the initial RT_{NDT} and shift in RT_{NDT} have been specified either deterministically or probabilistically, the critical stress intensity factor, $K_{Ic,}$ can be calculated. Figure H-3 shows a plot of K_{Ic} data versus $T\text{-}RT_{NDT}$, where T is the temperature of the material and RT_{NDT} is the sum of the initial RT_{NDT} and the shift in RT_{NDT}. Because K_{Ic} is a material property, it exhibits some variability and is treated as a random variable. A mean curve for K_{Ic} versus $T\text{-}RT_{NDT}$ was developed through regression analysis. The equation for this mean curve is:

$$\overline{K}_{Ic} = 36.2 + 49.4\exp(0.0104(T-RT_{NDT})) \quad \text{for } T-RT_{NDT} \leq -50°F \qquad \text{(H.5a)}$$

$$\overline{K}_{Ic} = 55.1 + 28.0\exp(0.0214(T-RT_{NDT})) \quad \text{for } T-RT_{NDT} > -50°F \qquad \text{(H-5b)}$$

If crack initiation is predicted, the crack may arrest as it runs deeper into the wall encountering hotter, less irradiated, and hence, tougher material. Arrest of the crack is predicted if

$$K_I < K_{Ia} \qquad \text{(H-6)}$$

where K_{Ia} is the stress intensity factor for crack arrest. Figure 5-4 shows the data for K_{Ia} versus $T\text{-}RT_{NDT}$ and a mean curve fit using regression analysis. The equation for the mean curve is:

$$K_{Ia} = 19.9 + 43.9\exp(.00993(T-RT_{NDT}) \quad \text{for } T-RT_{NDT} < 50°F \text{ (H-7a)}]$$

$$K_{Ia} = 70.1 = 6.5\exp(.0196(T-RT_{NDT}) \quad \text{for } T-RT_{NDT} > 50°F. \text{ (H-7b)}$$

Both the mean crack initiation and crack arrest toughnesses were truncated at an upper shelf value of 200 KSI $\sqrt{in}$. Thus if crack arrest is not predicted before K_I reaches a value of 200 KSI $\sqrt{in}$. vessel failure is predicted.

H.2.2 Simulation Model

Figure H-5 illustrates the simulation model developed for RPV failure probability. The left hand column in the figure is the deterministic analysis which includes the heat tansfer, thermal and pressure stress, and applied stress intensity value calculations for a range of crack depths at ten time steps in the transient. Matrices of temperature and K_I values are stored for use later in the simulation analysis.

The variables designated "simulate" in the diagram are treated as random variables, and their values are sampled from a statistical distribution defined by input parameters. As discussed in the previous section, crack, depth, a; fluence; RT_{NDTo}; copper content; K_{Ic}; and K_{Ia} were treated as random variables in this study. A value for each of these random variables is sampled from the appropriate statistical distribution. Once the flaw size is simulated, the corresponding K_I value is retrieved from the K_I matrix developed earlier in the code. The mean K_{Ic} value is calculated according to the equation (H-5) using the temperature corresponding to the time step and simulated crack depth and an RT_{NDT} based on the values of copper content, fluence, and RT_{NDTo} sampled from their corresponding statistical distributions. Since the K_{Ic} data exhibits significant variability, the K_{Ic} value is simulated by sampling from a distribution about the mean K_{Ic} value.

If crack initiation is predicted, the crack is allowed to advance through the RPV wall in discrete steps of 0.25 inches, and a check for crack arrest is made at each crack advance. K_{Ia} is treated in a similar fashion to K_{Ic} as mentioned above. If crack arrest is predicted, the code continues to analyze successive time steps in the transient using the arrested crack depth. Since the applied

K values and material temperature at the crack tip are a function of time in the transient, reinitiation of the crack may occur.

Each pass through the simulation loop depicted in Figure H-5 represents a single computer experiment conducted to determine if RPV failure will occur. Up to a million passes through this loop can be made. The code keeps track of the number of crack initiations and RPV failures and the probabilities of crack initiation and RPV failure are estimated by dividing these values by the total number of trials. Thus the code actually performs millions of deterministic calculations with each set of calculations based on a different set of values selected from the appropriate statistical distributions for the significant variables. This is equivalent to subjecting a population of up to a million operating reactor pressure vessels to the pressurized thermal shock transient of interest and then inferring the failure probability based on the number of observed failures.

H.2.3 Statistical Distributions of Random Variables

The simulation model described above suffers from the same problem as all analytic models, its output is only as good as its input. Unfortunately, very little information exists in the literature regarding the required statistical inputs, and the time frame of this initial study was not sufficient to allow the necessary research and analysis to develop rigorous statistical inputs. Therefore, many of the statistical distributions associated with the random variables in the model are based on expert opinion and have somewhat ill-defined "levels of confidence." It is appropriate to interject at this point that, because of the uncertainties associated with the input parameters, the best use of the results of this study is in a relative sense to assist in the decision-making process.

The number and size of cracks in the weld material of the RPV is probably the random variable with the greatest uncertainty. Several crack size distributions exist in the literature. These distributions are based on the experience of RPV fabricators and nondestructive examinations. The flaw distribution is of course difficult to quantify since the flaws of interest are not the flaws that have been detected, but those of unknown size and number that remain in the RPV because they were not detected. Figure H-6 shows the probability of having a flaw of depth a in a reactor pressure vessel longitudinal beltline weld as estimated in the OCTAVIA computer code. The weld volume associated with the OCTAVIA flaw distribution was defined as the volume of longitudinal weld material in the beltline region of a PWR. To obtain the flaw distribution, for a single beltline weld, as considered in this study, the OCTAVIA flaw distribution was adjusted assuming that the flaws were equally distributed among six longitudinal beltline. For illustration, the crack depth, a, in Figure H-6 is represented as a continuous random variable. However, in this study, the crack depth was used as a discrete random variable. For the curve in Figure H-4, approximately nine distinct crack depths ranging from 0.125 to 3.5 in. were used and the probabilities indicated at these crack depths were reduced by a factor of 1/6 to represent the probability of a flaw in one weld and were used to construct a stepwise cumulative probability distribution. The Monte Carlo simulation in the computer code used the stepwise cumulative distributions to generate a crack depth for each simulation cycle.

The distribution of RT_{NDTo} is dependent on the variability in the material and measurement error. In discussions with the metallurgists at materials testing laboratories, they indicated that they believed their accuracy in determining RT_{NDT} was $\pm 20°F$. No data exist from which to infer the shape of the distribution. Therefore, for a reference case, a normal distribution with a standard deviation of 15°F was assumed. Sensitivity studies were conducted assuming that the standard deviation was 30°F.

The variance in fluence is due to the power distribution in the reactor core and inaccuracies in calculation. Experts at Hanford Engineering and Development Laboratory in Richland, Washington, have estimated the uncertainty in fluence estimates to be on the order of $\pm$ 30% (1σ) using common practice techniques. For the reference case, a normal distribution with a standard deviation of 30% was assumed. Sensitivity studies were conducted assuming standard deviations of 50% and 15%.

A study was conducted to evaluate the sensitivity of the calculated failure probabilities to the tails associated with the normal distributions assumed for RT_{NDTo} and fluence. In this study the distributions were truncated at the mean plus and minus three standard deviations. The results indicated no appreciable difference, and it was concluded that the tails of the assumed normal distributions do not dominate in the calculations.

Copper was introduced into the welds of the RPV from welding rods that were copper coated to improve the welding process. Chemical composition analyses of welds from RPV prolongations have recently provided extensive dat for welds representative of those in operating plants. Rigorous statistical analysis of these data is not yet complete. However, the distribution does appear to be symmetrical with a standard deviation in the range of .02% to 0.5%. For the reference case, a normal distribution with a standard deviation of 0.025% was assumed. In the sensitivity studies a 0.07% standard deviation was considered. In all analyses the range of simulated copper content values was limited to 0.08% to 0.40% copper.

As described in Section H.2.1, K_{Ic} and K_{Ia} and also treated as random variables with a normal distribution and a 10% standard deviation about their respective means curves. Due to lack of sufficient data, the distribution of K_{Ic} and K_{Ia} about their mean is difficult to rigorously determine. However, several papers have suggested using a normal distribution about the mean with a standard deviation of 10%, and this distribution was assumed in generating the results presented here. The normal distribution about the mean was applied to both the transition and upper shelf toughness regions. Sensitivity studies were conducted to evaluate the sensitivity of the claculated failure probabilities to the assumed variability in K_{Ic} and K_{Ia}.

H.3 Results

This section presents results of a reference case and of certain sensitivity studies performed using the simulation model described in Section H.2. As stated earlier, due to uncertainties in the input data, it is suggested that these results be considered in a relative rather than an absolute sense. The sensitivity studies performed identify important parameters and their interaction and suggest how sensitive the reference case failure probabilities are to uncertainties in the input data. The results presented are conditional probabilities; that is, the probability of failure of a RPV weld given that the pressurized thermal shock transient under consideration occurs. To convert the results presented here into failure rates, the frequency of occurrence of the transient considered must be defined. Since the results presented are for an individual weld in the RPV beltline, the total conditional failure probability of the RPV beltline welds is the appropriate summation of the failure probabilities for each weld. If these values are sufficiently low and independence is assumed, the failure probabilities for the six welds can simply be summed. If the failure probabilities become high, the intersection of the weld failure probabilities must be subtracted.

H.3.1 Reference Case

The reference case analysis is defined as follows:

- The Rancho Seco transient (Figure H-7)
- The OCTAVIA flaw distribution,
- Copper $\sim N(\mu, 0.25\%)$,
- $RT_{NDTo} \sim N(\mu, 15°F)$,
- FLUENCE $\sim N(\mu, 30\%)$,
- ΔRT_{NDT} - HEDL mean curve, and
- K_{Ic} and K_{Ia} treated as random variables.

Figures H-8 through H-12 present the conditional failure probabilities calculated for the reference case condition. Each figure presents the failure probability versus the mean fluence for a specified mean copper content and three mean values of RT_{NDTo}. Also, plotted across the top of each figure, is the ΔRT_{NDT} calculated using the mean HEDL curve. These shifts are based on the mean copper content and fluence value in each figure. These curves make it possible to estimate the failure probability for the beltline region of a PWR for which the mean values of the random variables can be estimated.

Several important observations can be made regarding Figures H-8 through H-12. The first observation is that no failure probabilities less than 10^{-5} are calculated for any combination of mean fluence copper content, or RT_{NDTo}. This result occurs because the Rancho Seco transient will result in an applied K_I value greater than the assumed mean upper shelf toughness of 200 $KSI\sqrt{in.}$ for flaws of 3.0 inches or greater depth, and the probability of such flaws existing is nearly 10^{-5} in the flaw distribution assumed. Therefore, the lower limit on calculated failure probabilities would change for different transients, flaw distributions, or assumptions about the upper shelf toughness.

The second important observation is that any specified value of failure probability corresponds within a few degrees to a specific mean value of RT_{NDT}, independent of the copper content and fluence by which the RT_{NDT} value was achieved. For example in Figure H-8, based on a copper content of 0.34%, a failure probability of 2×10^{-5} corresponds to a mean RT_{NDT} value of approximately 255°F to 260°F for the three values of RT_{NDTo}. Similarly, in Figure H-11, based on a mean copper content of 0.28%, a failure probability of 2×10^{-5} corresponds to a mean RT_{NDT} of approximately 255°F for the two values of RT_{NDTo}. These results demonstrate that RT_{NDT} is in fact an excellent criterion for evaluating reactor pressure vessel integrity under specified thermal shock conditions. The mean RT_{NDT} value corresponding to a specific failure probability will, of course, be different for different pressurized thermal shock transients.

H.3.2 Reference Case Sensitivity Studies

Sensitivity studies were conducted on the distribution for copper content, initial RT_{NDT}, fluence, and fracture toughness. In addition, conditional failure probabilities were calculated assuming that specific flaw sizes exist with a probability of 1.0. Finally, a sensitivity study was conducted for a set of hypothetical transients with assumed expontial temperature decays and constant pressures. These cases are intended to provide insight into how sensitive RPV failure calculations are to thermal hydraulic parameters such as temperature, pressure, rate of cooldown, and heat transfer coefficient.

H.3.2.1 Copper Content

Figure H-13 illustrates the results of the sensitivity study on copper content. When the standard deviation for the copper distribution was increased from 0.025% to 0.07%, the calculated failure probabilities increased by approximately a factor of 5.

H.3.2.2 Initial RT_{NDT}

Figure H-14 illustrates the results of the sensitivity study on RT_{NDTo}. When the standard deviation for the RT_{NDTo} distribution was increased from 15°F to 25°F, the calculated failure probabilities were increased by a factor of approximately 3.

H.3.2.2 Fluence

Figure H-15 illustrates the results of the sensitivity study on fluence. The standard deviation for the fluence distribution was increased from 30% to 50% and decreased to 15%. The increased standard deviation resulted in approximately a factor of three increase in calculated failure probabilities, while the decrease in the standard deviation had little effect on the calculated failure probabilities.

H.3.2.4 Fracture Toughness

Figure H-16 illustrates the results of the sensitivity study on fracture toughness. Three different representations of the fracture toughness distribution were considered. In the first two cases the normal distribution about the mean fracture toughness values for K_{Ic} and K_{Ia} was maintained but the standard deviation was increased to 15% and then 20% of the mean value. In the third case K_{Ic} and K_{Ia} were treated deterministically using the lower bound fracture toughness curves from Section XI of the American Society of Mechanical Boiler and Pressure Vessel Code (see Figures H-3 and H-4). The sensitivity study was conducted for a mean copper content of 0.34% and a mean initial RT_{NDT} of 0°F. Assuming the larger standard deviations resulted in less than a factor of three difference from the reference case failure probabilities for a mean RT_{NDT} of 236°F or less. At higher values of RT_{NDT} the calculated failure probabilities for the assumed standard deviation of 15% and 20% were a factor of 50 and over an order of magnitude greater than the reference case, respectively. When the lower bound fracture toughness curves from Section XI of the Code were used, the calculated failure probabilities were one order of magnitude to almost two orders of magnitude higher than the reference case.

Results of intermediate scale tests conducted at Oak Ridge National Laboratory suggest that long cracks in large reactor vessels may exhibit "lower bound" fracture toughness. Several points should be made regarding this hypothesis. First, the "lower bound" performance was relative to fracture toughness data generated from small specimens not all of sufficient size to qualify as valid in accordance with ASTM-E-399 criteria. Second, cracks that exhibited "lower bound" performance in the ORNL tests were long flaws (≈38 inches), and shorter more realistic flaws are expected to exhibit toughness more closely represented by the toughness distribution assumed in the reference case. Finally, the intermediate scale tests performed have exhibited statistical variability in fracture toughness, but none of them have demonstrated fracture toughness as low as the ASME Code Section XI toughness curves.

The results of this sensitivity study show that the failure probabilities are sensitive to the distribution in fracture toughness, especially for mean values of RT_{NDT} greater than approximately 240°F. Thus, an effort should be made to better define this distribution. Experience to date suggests that fracture toughness may be a function of crack length as well as other parameters, and that in analyses assuming a bivariate flaw distribution of depth and length, it may also be appropriate to consider a relation between crack length and fracture toughness.

H.3.2.5 Simultaneous Increase in the Variability of All Random Variables

Figure H-17 presents the failure probabilities calculated when all the random variables were assumed to show the increased variances used in sensitivity studies, including one case where K_{Ic} and K_{Ia} were treated as random variables and one case where they were modelled using the lower bound curves. For the first case the calculated failure probabilities were approximately an order of magnitude greater than the reference case, while for the second case (lower bound K_{Ic} and K_{Ia}) the calculated failure probabilities were almost three orders of magnitude higher.

H.3.2.6 Flaw Distribution

Figure H-18 presents the conditional failure probabilities calculated assuming that flaw sizes ranging from 0.125 inches to 2.0 inches exist with a probability of 1.0 and for several different mean fluence values and values of RT_{NDT}.

The curves presented in Figure H-18 are useful because they can be used to calculate failure probabilities for different crack depth distributions. In Table H-1 the conditional failure probability is calculated for a reactor pressure vessel with mean copper content of 0.34% and mean initial RT_{NDT} of 0°F, assuming a flaw distribution less severe than the OCTAVIA distribution assumed in the reference case. The estimated failure probability for the less severe flaw distribution is 4.7×10^{-5} compared to 7.5×10^{-5} for the OCTAVIA distribution. The relatively small difference in the estimated failure probabilities results because the flaw distributions considered are not significantly different in the range of flaw depths that contribute most to the failure probability. An advantage of this approach to evaluating sensitivity to the assumed flaw distribution is that it allows easy identification of the range of flaw depths that contribute most signficantly to the failure probability.

H.3.2.7 Shift in RT_{NDT}

A sensitivity study was conducted using the fluence versus ΔRT_{NDT} relation from Regulatory Guide 1.99, "Effects of Residual Elements in Predicted Damage to Reactor Vessel Materials." Use of the upper bound trendlines presented in Regulatory Guide 1.99 is not considered appropriate in a probabilistic analysis but was considered in this sensitivity study in an effort to quantify the effect of differences in assumed trendlines. Figure H-19 presents the results generated assuming ΔRT_{NDT} as predicted by the HEDL trendlines and the Regulatory Guide 1.99 trendlines. Assuming the more severe Regultory Guide 1.99 trendlines increased the calculated failure probabilities by a maximum of nearly two orders of magnitude.

H.3.2.8 Upper Shelf

As discussed in Section H.2.1, the results presented in this study are based on linear elastic fracture mechanics analysis. In the transients of interest, however, linear elastic fracture mechanics may not be valid when cracks are predicted to run deep into the vessel wall where the material is operating in the upper shelf temperature regime. In the upper shelf temperature regime, crack extension generally occurs in a ductile mode referred to as tearing rather than in a cleavage mode as predicted by linear elastic fracture mechanics. In the reference case analysis, the mean fracture toughness curves were truncated at an upper value of 200 KSI$\sqrt{in.}$, and it was assumed that if crack arrest did not occur before the applied K_I reached 200 KSI$\sqrt{in.}$, the crack would tear through the wall. In reality this problem requires an elastic-plastic or tearing instability type of analysis which has not yet been fully developed and validated for pressurized thermal shock conditions. A study was conducted to evaluate the sensitivity of the calculated failure probabilities to the assumed upper shelf value. In this study the mean upper shelf value was increased to 300 KSI$\sqrt{in.}$, 400 KSI$\sqrt{in.}$ and infinity and a check was incorporated for plastic instability of the remaining section. The assumed higher upper shelf toughness values all resulted in the same calculated failure probabilities, as illustrated in Figure H-20. The calculated failure probabilities with the increased upper shelf values are more than an order of magnitude less than the reference case failure probabilities for mean values of RT_{NDT} less than approximately 240°F. At a mean RT_{NDT} value of 250°F the failure probability associated with the increased upper shelf toughnesses is approximately a factor of four less than the reference case; and at a mean RT_{NDT} value of 275°F or greater the calculated failure probabilities are the same. Thus upper shelf material behavior may decrease the probability of catastrophic vessel failure for mean RT_{NDT} values of 250°F or less but provides very little additional margin at higher values of RT_{NDT}. Two notes of caution are in order. First, recent information suggests that the gradient in fluence attenuation may not be as steep as assumed in these analyses, and a different model assuming greater radiation damage deeper in the vessel wall may bring the refernece case and increased upper shelf toughness failure probabilities closer together at a lower value of mean RT_{NDT}. Second, the calculated probabilities of crack initiation, which is significant from an economic point of view, are unaffected by the assumption regarding upper shelf toughness.

H.3.2.9 Cladding

For surface cracks as assumed in this evaluation, the stainless steel cladding will increase the applied stress intensity value due to differential thermal expansion between the clad and base metal. This effect has not yet been included in the fracture mechanics code used in the probabilistic analysis, although it has been evaluated deterministically. A study was conducted to estimate the magnitude of the effect of the increased K_I due to cladding on the calculated failure probabilities. In this study the thermal component of the applied stress intensity factor, K_{It}, was increased by 10% and 20%. This is a gross approximation since the actual increase in K_I will be a function of crack depth and time in the transient. However, calculations indicate that for the Rancho Seco transient the maximum contribution to the thermal component of the applied K_I is less than 10%. Therefore, the case of a 10% increase in K_{It} should be bounding for the Rancho Seco transient as analyzed deterministically. The case of a 20% increase in K_{It} gives some insight into sensitivity of the assumptions regarding initial stress in the cladding at normal operating temperature. The results of the study are presented in Figure H-21. For an increase in K_{It} of 10% there is essentially no change in the calculated failure probabilities for mean surface RT_{NDT} values less than approximately 250°F. Above a mean RT_{NDT} of 250°F the failure probabilities increase by less than a factor of three. Figure H-22 illustrates the factor of increase in conditional failure probability assuming a 10% increase in the thermal component of the applied stress intensity factor due to the affect of cladding. For a 20% increase in K_{It} the calculated failure probabilities increase by a maximum factor of approximately 4.

It should be noted that the differential thermal effect between the cladding and base metal may be more significant for more severe thermal shocks, and caution must be exercised in extending the results of this study to those transients.

H.3.3 Transient Sensitivity Studies

In addition to the reference Rancho Seco transient, postulated MSLB and turbine trip with stuck-open bypass valve transients were evaluated using the probabilistic code. The same transients were analyzed deterministically by ORNL in Reference H.1 and were selected for probabilistic analysis to provide some estimate of the conservatisms in the deterministic calculations. Also, a set of hypothetical pressurized thermal shock transients with assumed exponential temperature decays and constant pressure levels was analyzed to determine the sensitivity of failure probability to the minimum temperature reached in the transient, rate of temperature drop, pressure level, and heat transfer coefficient.

H.3.3.1 Main Steamline Break and Turbine Trip With Stock Open By-pass Value Transients

Figures H-23 and H-24 present the pressure and temperature time histories associated with the postulated MSLB and stuck-open bypass valve transients, respectively. The solid lines in the figures represent the pressure and temperature time histories calculated by Brookhaven National Laboratory using the IRT Code. Reference H.1 provides details of the assumptions made in performing the thermal hydraulic calculations. The solid lines in each figure represent the pressure and temperature time histories calculated by the IRT analysis. The dashed lines represent the fourth order polynomial fits to the IRT pressure and temperature time histories used for performing closed form heat transfer and stress analyses. The applied stress intensity values resulting from these polynomial fits agree well with those calculated by ORNL using the OCA-1 numerical heat transfer analysis. Figures H-25 and H-26 present the calculated failure probabilities for the MSLB and stuck-open bypass valve, respectively, for a longitudinal beltline weld with a mean initial RT_{NDT} of 0°F and mean copper contents of 0.22% and 0.34%. The failure probabilities are very high for both of these severe thermal transients.

H.3.3.2 Hypothesized Transients with Exponential Cooldowns and Constant Pressures

Table H-2 presents the failure probabilities for a set of hypothesized pressurized thermal shock transients. The temperature time history in each transient is assumed to follow an exponential decay defined by

$$T(t) = T_F + (550 - T_f)e^{-\beta t}$$

where T is the temperature in °F, t is time in minutes, T_f is the final temperature of the transient in °F, and β is the decay constant in min^{-1}. Three values of T_f, 150°F, 225°F, and 300°F; three values of β, 0.05 min^{-1}, 0.15 min^{-1}, and 0.50 min^{-1}; and five constant pressure levels, 0 psig, 500 psig, 1000 psig, 1500 psig, and 2000 psig were considered for a total of 45 different transients. Each of these transients was then evaluated for five levels of fluence, 0.5 10^{19} neut/cm², 1.0 x 10^{19} neut/cm², 2.0 x 10^{19} neut/cm², 3.0 x 10^{19} neut/cm², and 4.0 x 10^{19} neut/cm² assuming a mean copper content of 0.30% and a mean initial RT_{NDT} of 20F. The data presented in Table H-1 have been used to evaluate the sensitivity of failure probability to the normalizing factor T_f - RT_{NDT}, β, and pressure.

H.3.3.2.1 T_f-RT_{NDT} Sensitivity Study

Figure H-27 presents failure probability versus T_f - RT_{NDT} for the three different values of β considered and a constant pressure of 1000 psig. An ideal normalizing factor would combine the significant transient parameters in such a way that one curve of failure probability versus the normalizing factor could be used to estimate the probability of failure for any arbitrarily defined transient. Several factors combining T_f, β, pressure, total temperature drop, and RT_{NDT} were considered but no combination of these factors yielded a perfect normalizing factor. However, for the range of transients considered here, T_f - RT_{NDT} is a fairly effective normalizing factor for any specific β and constant pressure level. Figure H-28 indicates that failure probability is highly sensitive to the value of T_f -RT_{NDT}. For example, considering a β of 0.15 min^{-1}, a decrease in T_f - RT_{NDT} from -20°F to -70°F results in a factor of approximately 150 increase in failure probability.

H.3.3.2.2 Cooldown Rate Sensitivity Study

Figure H-27 indicates a much greater increase in failure probabilities when β is increased from 0.05 to 0.15 than when β is increased from 0.15 to 0.50. This observation is more clearly illustrated in Figure H-28 where failure probability is plotted as a function of β for several values of T_f-RT_{NDT} and 1000 psig constant pressure. The curves illustrate that failure probability is very sensitive to β in the range below 0.15 min^{-1} while increasing β beyond 0.15 min^{-1} increases the failure probability by less than a factor of five. This is most likely a result of the assumed thermal inertia of the system, and the sensitvity curves will change if different thermal characteristics are assumed in the heat transer analysis.

H.3.3.2.3 Pressure Sensitivity Study

Figure H-29 is a plot of failure probability versus pressure for several values of the parameter T_f - RT_{NDT}. The figure illustrates increasing sensitivity to pressure as the paramter T_f - RT_{NDT} increases. For example, for a T_f-RT_{NDT} value of -25°F an increase in pressure from 500 psig to 2000 psig results in approximately a factor of 200 increase in failure probability while a similar pressure increase for a T_f - RT_{NDT} value of -120°F increases the failure probability by only a factor of 5. Thus pressure is a more important parameter in the transients where the minimum temperature is near the value of RT_{NDT} rather than well below it. It should be noted that for a pressure level of 0.0 psig, the failure probability is zero. Thermal Shock Experiment 6 recently completed at ORNL demonstrated that although severe cracking may occur under the condition of no pressure, thermal stesses alone are not sufficient to drive a crack through the RPV wall.

H.3.3.2.4 Heat Transfer Coefficient Sensitivity Study

Figure H-30 presents the results of a sensitivity study conducted on heat transfer coefficient. The two curves in the figure present RPV failure probability versus heat transfer coefficient, h in BTU/hr/ft^2 °F, for two different hypothetical exponential cooldowns. One has a final transient temperature of 150°F while the other has a final transient temperature of 200°F. A constant pressure level of 1000 psig was assumed and the RPV material was assumed to have an adjusted RT_{NDT} of 250°F. When the thermal conductivity of the cladding is considered, the range of the effective heat transfer coefficient for the thermal hydraulic transients under consideration is between 200 BTU/hr/ft^2 °F and 400 BTU/hr/ft °F. The results indicate that over that range, the assumed heat transfer coefficient can make as much as an order of magnitude difference in the calculated RPV failure probabilities. The results presented in this study were generated assuming an effective heat transfer coefficient of approximately 300 BTU/hr/ft^2. The assumed thermal diffusivity in this study was 0.98 in^2/min and a constant value of 0.332 was used for the parameter (E*ALPHA)/(1-MV). Where E is Young's Modulus, ALPHA is the coefficient of thermal expansion, and MV is Poisson's ratio.

X

H.3.4 Inservice Inspection Sensitivity Study

Sensitivity studies were conducted using Figure H-18 to evaluate the effect of various levels of non-destructive examination (NDE) reliability on reactor pressure vessel failure probability. Three different functions of flaw non-detection probability were considered. The first function for probability of flaw non-detection was taken from Reference H.2. This function was based on a survey of NDE experts. The other two flaw nondetection probability functions assumed probabilities of non-detection of 0.5 and 0.05, respectively, over the entire range of crack depths. The latter two functions were selected primarily for the purpose of evaluating the sensitivity of failure probability to a wide range of NDE reliabilities. However, they were also intended to correspond to condition of rough surface finish and smooth surface finish, respectively. It was assumed for all functions of NDE reliability that cracks of greater than 2.0 inches in depth would be detected with certainty. The results of these evaluations are presented in Tables H-3A through H-3C. The first column in these tables gives the flaw depth, a, in inches; the second column is the probability of existence of a crack of depth a as estimated by the OCTAVIA flaw distribution; column three is the probability of non-detection; column four is the probability of existence of a crack of depth a after performing an NDE (the product of columns two and three); column five is the conditional probability of failure given the Rancho Seco transient and existence of a crack of depth a; and column six is the contribution to the conditional failure probability of the reactor vessel weld for each crack depth (the product of columns four and five). The conditional failure probability of the reactor vessel weld given that the Rancho Seco transient occurs is given by the sum of the probabilities in column six.

The conditional failure probabilities of a reactor pressure vessel weld following inservice inspection can be compared to the conditional failure probability of 7.5×10^{-5} before the inservice inspection, from Figure H-8. This comparison indicates inservice inspections conducted with reliabilities corresponding to the Reference H.2 report probability of non-detection function or the constant 0.5 probability of non-detection problem will do very little to improve reactor pressure vessel reliability under pressurized thermal shock conditions. However, if a probability of non-detection of 0.05 can be achieved, even for small flaws, then a substantial decrease in failure probability, approximately a factor of 20, will result.

H.3.5 Warm Prestressing Sensitivity Study

A study was conducted to determine the effects of warm prestressing on the calculated conditional failure probabilities for the idealized Rancho Seco transient that was considered as the reference transient in Section H.3.1. The warm prestress phenomenon was modelled by simply not allowing crack initiation at any time step in the transient for which the applied K value for the simulated crack depth was greater at the previous time step. No allowance was made for a possible increase in the allowable K_I to K_{Ic} ratio above 1.0 resulting from warm prestressing.

For the Rancho Seco transient warm prestressing was very effective in inhibiting crack extension. The conditional failure probabilities calculated assuming warm prestressing were less than 10^{-5} for mean RT_{NDT} values less than 290°F. (See Table H-3.)

TABLE H-1: FLAW DISTRIBUTION SENSITIVITY STUDY

a (INCHES)	$P(a)$	$P(F\|a)$	$P(F) = P(F\|a)\,P(a)$
0.125	—	0	0
0.25	4×10^{-2}	1.5×10^{-4}	4×10^{-6}
0.50	3×10^{-3}	1.0×10^{-2}	3×10^{-5}
1.00	2×10^{-4}	5.4×10^{-2}	1×10^{-5}
1.50	1×10^{-5}	5.6×10^{-2}	6×10^{-7}
2.00	1×10^{-6}	4.5×10^{-2}	5×10^{-8}

CONDITIONAL FAILURE PROB: 4.7×10^{-5}

Table H-2 $Cu_{\mu} = 0.30$ $Cu_{\sigma} = 0.025$ $F_{\sigma} = 30\%$ Ni = .75

$IRT_{NDT\mu} = 20°F$. $IRT_{NDT\sigma} = 15°F$ OCTAVIA FLAW DISTRIBUTION

HEDL MEAN ΔRT_{NDT} $T = T_f + (T_i - T_f)e^{-\beta t}$

Pressure (psi)	T_f / β — Fluence (neut/cm²)	150°F .05	150°F .15	150°F .50	225°F .05	225°F .15	225°F .50	300°F .05	300°F .15	300°F .50
0	0.5×10^{19}	0			0	0	0	0	0	0
	1.0×10^{19}	0			0	0	0	0	0	0
	2.0×10^{19}	2×10^{-6}			0	0	4×10^{-6}	0	0	0
	3.0×10^{19}	5.8×10^{-5}			0	0	1×10^{-5}	0	0	0
	4.0×10^{19}	2.1×10^{-4}	3.1×10^{-2}	9.1×10^{-2}	0			0	0	0
500	0.5×10^{19}	0		8.3×10^{-2}	0	0	0	0	0	0
	1.0×10^{19}	2.2×10^{-5}	2.6×10^{-3}	1.1×10^{-2}	0	0	0	0	0	0
	2.0×10^{19}	2.9×10^{-4}	2×10^{-2}	6.3×10^{-2}	0	1×10^{-5}	4.8×10^{-5}	0	0	0
	3.0×10^{19}	9.3×10^{-4}	5×10^{-2}	1.3×10^{-1}	0	9×10^{-5}		0	0	0
	4.0×10^{19}	1.7×10^{-3}	8.3×10^{-1}	2.1×10^{-1}	0	4.3×10^{-4}	2.3×10^{-3}	0	0	0
1000	0.5×10^{19}	2.4×10^{-5}	1.2×10^{-3}	4.1×10^{-3}	0	2×10^{-6}	4×10^{-6}	0	0	0
	1.0×10^{19}	2.6×10^{-4}	1×10^{-2}	3×10^{-2}	0	2×10^{-6}	8×10^{-6}	0	0	0
	2.0×10^{19}	1.7×10^{-3}	4.9×10^{-1}	1.2×10^{-1}	4×10^{-6}	1.3×10^{-4}	5×10^{-4}	0	0	0
	3.0×10^{19}	5.1×10^{-3}	9.8×10^{-2}	2.2×10^{-1}	3.2×10^{-4}	8.1×10^{-4}	3.4×10^{-3}	0	0	0
	4.0×10^{19}	1.1×10^{-2}	1.5×10^{-1}		7×10^{-5}	2.6×10^{-3}	9.6×10^{-3}	0	0	2×10^{-6}
1500	0.5×10^{19}	1.9×10^{-4}	3.8×10^{-3}	1×10^{-2}	4×10^{-6}	1×10^{-5}	1.4×10^{-5}	0	2×10^{-6}	4×10^{-6}
	1.0×10^{19}	1.1×10^{-3}	2.5×10^{-2}	5.6×10^{-2}	0	2.8×10^{-5}	6.6×10^{-5}	0	2×10^{-6}	4×10^{-6}
	2.0×10^{19}	$6.2 \times 10^{-?}$	9.1×10^{-2}	1.9×10^{-1}	2.8×10^{-5}	6.1×10^{-4}	2×10^{-3}	0	2×10^{-6}	4×10^{-6}
	3.0×10^{19}	1.7×10^{-2}	1.6×10^{-1}		1.8×10^{-4}	3×10^{-3}	9.1×10^{-2}	0	4×10^{-6}	8×10^{-6}
	4.0×10^{19}	3.4×10^{-2}	2.4×10^{-1}		4.4×10^{-4}	8.2×10^{-3}	2.3×10^{-2}	0	$1.2 \times \cancel{10}^{-5}$ 16	$3.2 \times 10^{-\cancel{6}5}$
2000	0.5×10^{19}	5.5×10^{-4}	1.0×10^{-2}	2.1×10^{-2}		2×10^{-5}	3.6×10^{-5}	2×10^{-6}		1×10^{-5}
	1.0×10^{19}	2.7×10^{-7}	4.9×10^{-2}	9.4×10^{-2}	1.8×10^{-5}	$9 \times 10^{-\cancel{6}5}$	2.7×10^{-4}	2×10^{-6}	1×10^{-3}	1.4×10^{-5}
	2.0×10^{19}	1.6×10^{-2}	1.5×10^{-1}	2.8×10^{-1}	1.7×10^{-4}	2.1×10^{-3}	5.1×10^{-3}	2×10^{-6}	1×10^{-5}	1.4×10^{-5}
	3.0×10^{19}	4.0×10^{-2}	2.6×10^{-1}		6.7×10^{-4}	8.7×10^{-3}	2×10^{-3}	2×10^{-6}	2×10^{-3}	3×10^{-5}
	4.0×10^{19}	6.6×10^{-2}	2.6×10^{-1}		1.6×10^{-3}	2.1×10^{-2}	4.4×10^{-2}	1×10^{-5}	$\cancel{1.2}$ 6.8 $\times 10^{-\cancel{5}3}$	1.2×10^{-4}

TABLE H-3A: A Marshall Report Probability of Nondetection

a	P(a)	P(Non-Detection)	P(a Inspection)	P(Failure)	P(Failure)
0.125	0.83	.69	.57	0	0
0.25	0.16	.49	.78	5×10^{-5}	3.9×10^{-6}
0.50	4.2×10^{-3}	.24	1.0×10^{-3}	1.0×10^{-2}	1.0×10^{-5}
1.00	4.1×10^{-4}	.061	2.5×10^{-5}	5.4×10^{-2}	1.4×10^{-6}
1.50	1.3×10^{-4}	.018	2.3×10^{-6}	5.6×10^{-2}	1.3×10^{-7}
2.00	4.2×10^{-5}	8.1×10^{-3}	3.4×10^{-7}	4.5×10^{-2}	1.5×15^{-8}
			CONDITIONAL FAILURE PROBABILITY		1.5×10^{-5}

TABLE H-3B Constant 0.50 Probability of Non-Detection

a	P(a)	P(Non-Detection)	P(a 1 Inspection)	P(Failure)	P(Failure)
0.125	0.83	0.50	0.42	0	0
0.25	0.16	0.50	0.08	1.5×10^{-4}	1.2×10^{-5}
0.50	4.2×10^{-3}	0.50	2.1×10^{-3}	1.0×10^{-2}	2.1×10^{-5}
1.00	4.1×10^{-4}	0.50	2.1×10^{-4}	5.4×10^{-2}	1.1×10^{-5}
1.50	1.3×10^{-4}	0.50	6.5×10^{-5}	5.6×10^{-2}	3.6×10^{-6}
2.00	4.2×10^{-5}	0.50	2.1×10^{-2}	4.5×10^{-2}	9.5×10^{-7}
			CONDITIONAL FAILURE PROBABILITY		4.9×10^{-5}

TABLE H-3C: Constant 0.05 Probability of Non-Detection

a	P(a)	P(Non-Detection)	P(a 1 Inspection)	P(Failure)	P(Failure)
0.125	0.83	0.05	4.2×10^{-2}	0	0
0.25	0.16	0.05	8.0×10^{-3}	1.5×1^{-4}	1.2×10^{-6}
0.50	4.2×10^{-3}	0.05	2.1×10^{-4}	1.0×10^{-2}	2.1×10^{-6}
1.00	4.1×10^{-4}	0.05	2.1×10^{-6}	5.4×10^{-2}	1.1×10^{-7}
1.50	1.3×10^{-4}	0.05	6.5×10^{-6}	5.6x10-2	3.6×10^{-7}
2.00	4.2×10^{-5}	0.05	2.1×10^{-6}	$4.5\times10\text{-}10^{-2}$	9.5×10^{-8}
			CONDITIONAL FAILURE PROBABILITY		3.8×10^{-6}

Based on the above studies it can be concluded that for transients whose thermal hydraulic characteristics ensure warm prestressing conditions, the probability of RPV failure can be significantly reduced.

H.3.6 Flaw Orientation Sensitivity Study

Results presented thus far have concentrated on the longitudinally oriented beltline welds. The volume and orientation of weld material in the reactor vessel beltline region depends on whether the beltline shell was fabricated from rolled plates or forged rings as illustrated in Figure H-1. Several operating vessels are fabricated from ring forgings or have limiting values of RT_{NDT} associated with circumferential welds.

The orientation of the beltline welds is significant in the evaluation of pressurized thermal shock transients because flaws oriented in a circumferential direction have a lower propensity for extension than those oriented parallel to the longitudal axis of the vessel. The circumferentially oriented crack has a lower propensity for crack extension because it is subject to a pressure stress only half as great as the longitudinal flaw and because the applied stress intensity factor is lower due to the increased bending stiffness of the cylinder about its azimuthal axis. In addition, these two factors also create a greater propensity for crack arrest in a circumferentially oriented flaw. Because flaws in the weld material are generally assumed to be oriented in the direction of the weld, reactor vessels fabricated from forged rings with circumferential welds are expected to have a greater tolerance for pressurized thermal shock loadings than reactor vessels fabricated from rolled plates with longtudinal welds.

A study was conducted to evaluate the relative differences in integrity between longitudinally and circumferentially oriented welds. Both determinstic and probablistic calculations were performed for two different transients. The transients were the idealized Rancho Seco Transient illustrated in Figure H-7 and the MSLB accident illustrated in Figure H-24. Two dimensional (infinitely long longitudinal and 360° circumferential) flaws were evaluated using linear elastic fracture mechanics analysis.

The results of the deterministic calculations indicate that the Rancho Seco transient will not cause catastrophic failure of the reactor pressure vessel for a surface RT_{NDT} less than 350°F (calculated by R.G. 1.99). For a surface RT_{NDT} of 350°F or lower, the deterministic calculations predict crack arrest less than halfway through the vessel wall in the linear elastic regime. For a surface RT_{NDT} of 370°F, crack arrest is predicted approximately three-fourths of the way through the vessel wall. Although some margin still exists for circumferentially oriented flaws, this depth of crack extension is approaching the condition where the vessel would fail due to plastic instability of the remaining ligament. Furthermore, this amount of crack extension leaves little margin for tearing of the crack which could occur in low upper shelf materials.

The probabilistic analysis of the Rancho Seco transient generally supports the conclusions from the deterministic calculations. The failure probabilities calculated for the Rancho Seco transient assuming that a 1.0-inch flaw existed with certainty were less than 10^{-5} for a mean surface RT_{NDT} values of 275°F or less and approximately 3.2×10^{-5} for mean surface RT_{NDT} of 290°F. Comparable failure probabilities for longitudinally oriented flaws were 7.5×10^{-4}, 10^{-2}, and 4.5×10^{-2} for mean surface RT_{NDT} values of 250°F, 275°F, and 290°F, respectively. Thus, for the Rancho Seco transient, the failure probability of a circumferentially oriented flaw is at least three orders of magnitude less than that of a longitudinally oriented flaw for mean surface RT_{NDT} values of 290°F or less. A comparison of the crack initiation probabilities for longitudinal and circumferential flaws indicated that the probability of initiation of a circumferential flaw ranges by approximately a factor of 1000 to 25 less than that of a longitudinal flaw for a corresponding range in mean surface RT_{NDT} values of 215°F to 290°F.

Deterministic calculations for the MSLB indicate that vessel failure due to extension of circumferential cracks will occur at RT_{NDT} surface values of 226°F (calculated by Regulatory Guide 1.99) or greater. Since 226°F was the lowest RT_{NDT} evaluated, vessel failure might be predicted at even lower values of RT_{NDT}. The probabilistic analysis of the MSLB indicated that the probability of failure of a circumferentially oriented flaw can be as little as a factor of 12 to 3 less than that for a longitudinal flaw for a corresponding range in mean surface values of RT_{NDT} between 250°F and 290°F. Figure H-32 presents the factor decrease in failure probability for circumferentially versus longitudinally oriented flaws of 1.0-inch and 0.5-inch depths. For a mean surface RT_{NDT} value of 215°F, no failures were generated in the simulation analysis. The probability of crack initiation for the postulated MSLB accident was essentially equal over a range in mean surface RT_{NDT} values of 215°F to 290°F for the flaw sizes considered.

In summary, both deterministic and probabilistic evaluations indicate that for transients as severe as those which have been observed (the Rancho Seco transient being considered the most severe) circumferential flaws will not lead to catastrophic vessel failure for relatively high values of RT_{NDT}. Furthermore, the probability of initiation of circumferentially oriented flaws is significantly less than that of longitudinal flaws until relatively high values of RT_{NDT} are reached. However, for much more severe postulated transients, deterministic analyses predict that catastrophic vessel failure can result from circumferentially oriented flaws at relatively low values of RT_{NDT}. In addition, probabilistic analyses indicate a relatively small difference in failure probabilities between circumferential and longitudinal flaws and essentially no difference in the probability of crack initiation for more severe transients.

H.4 Application of Probabilistic Analyses in Establishing Regulatory Criteria

Probabilistic analysis is a very powerful technique for gaining insight and understanding of complex technical issues and when used correctly can result in effective regulation without excessive conservatism. However, misapplication of the results of probabilistic analyses which may occur due to inadequate understanding of the bases upon which they were developed could compromise safety and economic objectives. In this context, the purpose of this section is to identify soem of the limitations of the work performed can be most approximately used in developing a regulatory position on the pressurized thermal shock issue.

and to define how the results presented [illegible]

H.4.1 Limitations of Probabilistic Fracture Mechanics Analyses

As indicated in Section H.2.3, the statistical distributions used to generate the results presented in Section H.3 are based largely on expert opinion and are subjective in nature. Efforts are currently in progress to assemble improved data bases and develop more rigorous statistical distributions. However, results generated using improved input data will not be available to assist in developing a short-term position on the pressurized thermal shock issue. Uncertainty in the statistical distributions used in the model is one of the main reasons for conducting the sensitivity studies presented in Section H.3.2. The results of these sensitivity studies, in which the variability and form of the statistical distributions were varied, indicate that uncertainties in the statistical distributions for copper content, initial RT_{NDT}, and fluence could contribute as much as an order of magnitude uncertainty to the results presented in Section H.3.

Flaw depth is the random variable with the greatest uncertainty. The sensitivity studies on flaw depth distribution and inservice inspection indicate that the calculated failure probabilities for the Rancho Seco transient are relatively insensitive to changes in the distribution for crack depths greater than approximately one inch. This is because relatively small flaws can dominate the failure probability due to the nature of the stresses and toughness gradient associated with pressurized thermal shock events. The sensitivity studies also indicate that the calculated failure probabilities could change substantially given a significant change in the distribution of crack depths. When the probabilities of all crack depths are altered by a constant factor, the calculated failure probabilities change by approximately the same factor. Thus, the uncertainty in the calculated failure probabilities is directly related to the uncertainty in the same crack distribution. Unfortunately, little data exist from domestic operating reactor vessels that would allow a rigorous determination of the flaw depth distribution, particularly in the range of crack depths less than one inch. The distribution of crack depths has large uncertainty associated with it and could easily contribute plus or minus an order of magnitude or more uncertainty to the calculated failure probabilities.

The sensitivity studies conducted on fracture toughness indicate that the calculated failure probabilities are very sensitive to the assumed variability in the fracture toughness data. At high values of RT_{NDT}, relatively small increase in the variabiilty of the fracture toughness can increase the calculated failure probabilities by well over an order of magnitude.

The above discussion suggests that the calculated failure probabilities could be underestimated due to uncertainties in copper content, initial RT_{NDT}, and fluence; the calculated failure probabilities could be over or underestimated due to uncertainty in the crack depth distribution; and the calculated failure probabilities could be underestimated due to uncertainties in the fracture toughness distribution. In addition to these uncertainties, there exist uncertainties due to elements not considered in the probabilitistic model. Specifically, the toughness of the stainless steel cladding which may be great enough to inhibit the initiation of small flaws and warm pre-stressing which may inhibit crack extension were not considered. If, in fact, the vessel cladding does maintain high toughness in the range of fluence levels of interest, the extension of finite elapsed cracks could be inhibited and the failure probabilities may be greatly over estimated. Similarly, warm pre/stressing which will be effective for a large class of pressurized thermal shock events would greatly reduce the calculated failure probabilities for such events.

Work is continuing to better quantify the confidence levels that can be associated with the calculated failure probabilities. However, based on the currently available data and analysis, it appears that plus or minus two orders of magnitude is a reasonable estimate of the uncertainty associated with the calculated failure probabilities.

H.4.2 Application of the Probabilistic Fracture Mechanics Results

The discussion of the previous section suggests that the results which have been presented are most appropriately used in a relative sense for identifying significant variables and variable interactions. Because of the uncertainties associated with the calculated failure probabilities, use of the results in an absolute sense to establish an RT_{NDT} screening limit would be inappropriate. Nonetheless, there does exist a tendency to view the results in an absolute sense when evaluating proposed regulatory requirements. Furthermore, there is a desire to view the results in an absolute sense when performing a probabilistic risk assessment. Utilization of the results in these manners is useful in evaluating a regulatory position, but the limitations of the analysis as discussed in the previous section must be kept in mind.

In conclusion, it is suggested that the regulatory criteria should be based on deterministic fracture mechanics analyses and that the probabilistic analyses not be used as the basis for developing such criteria until such time as greater confidence in the probabilistic analyses can be attained. It is suggested, however, that the probabilistic analyses be used, with caution, to check deterministically derived criteria relative to desired margins of safety.

REFERENCES

H.1 Kryter, R. C. et al. 1981. Evaluation of Pressurized Thermal Shock. NUREG/CR-2083, ORNL/TM-8072, Oak Ridge National Laboratory, Oak Ridge, Tennessee.

H.2 Study Group Report, "An Assessment of the Integrity of PWR Pressure Vessels," UKAEA Report October 1976.

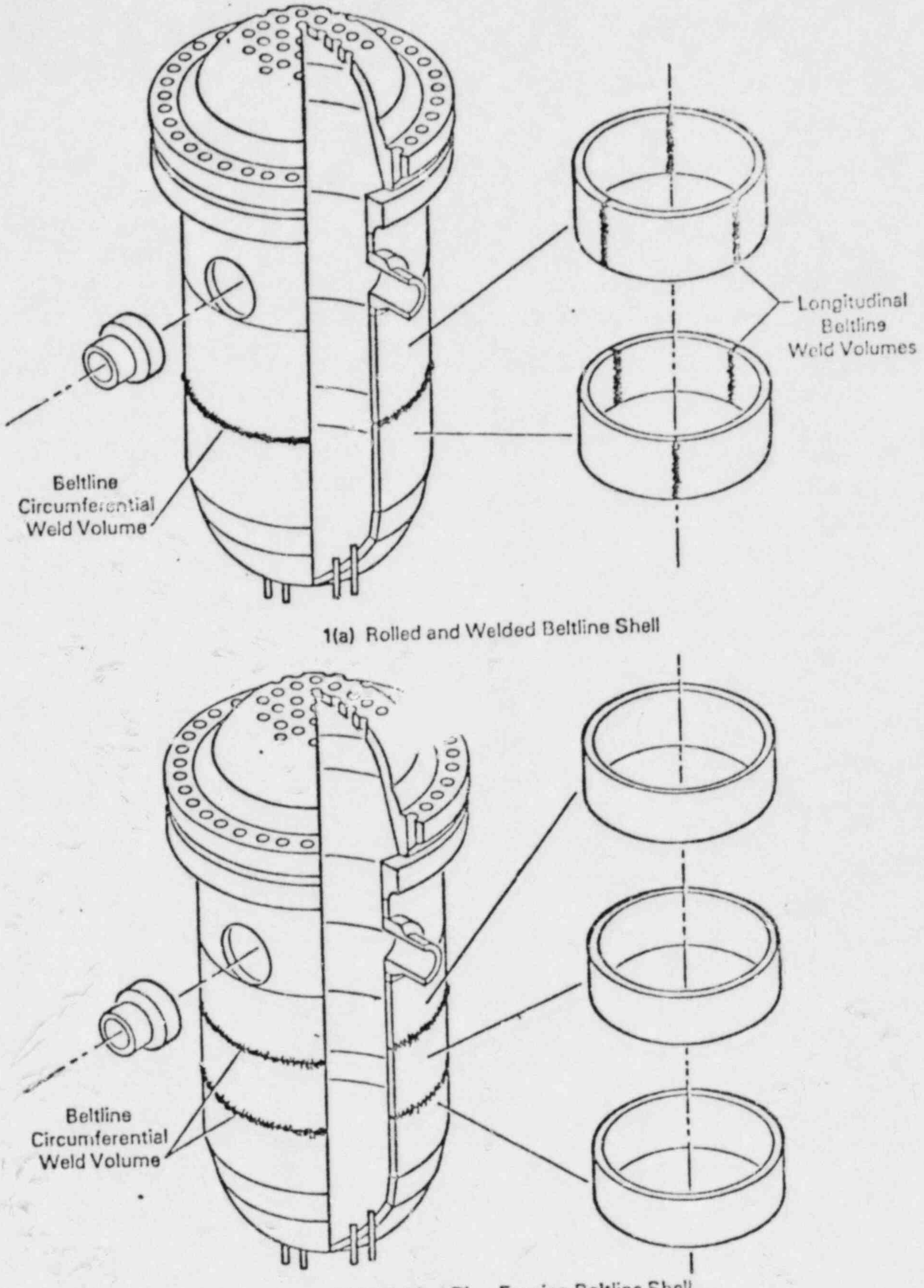

FIGURE H-1 PWR Beltline Shell Fabrication Configurations

NIEL RANDALL PWR DATA BASE FOR FLUENCE vs ΔRT_{NDT}
REGRESSION ANALYSIS PERFORMED BY HEDL

$$\Delta T = \left[-4.83 + 476\cdot Cu + 267\cdot Cu\cdot Ni\right]\left[F/10^{19}\right]^{0.218}$$

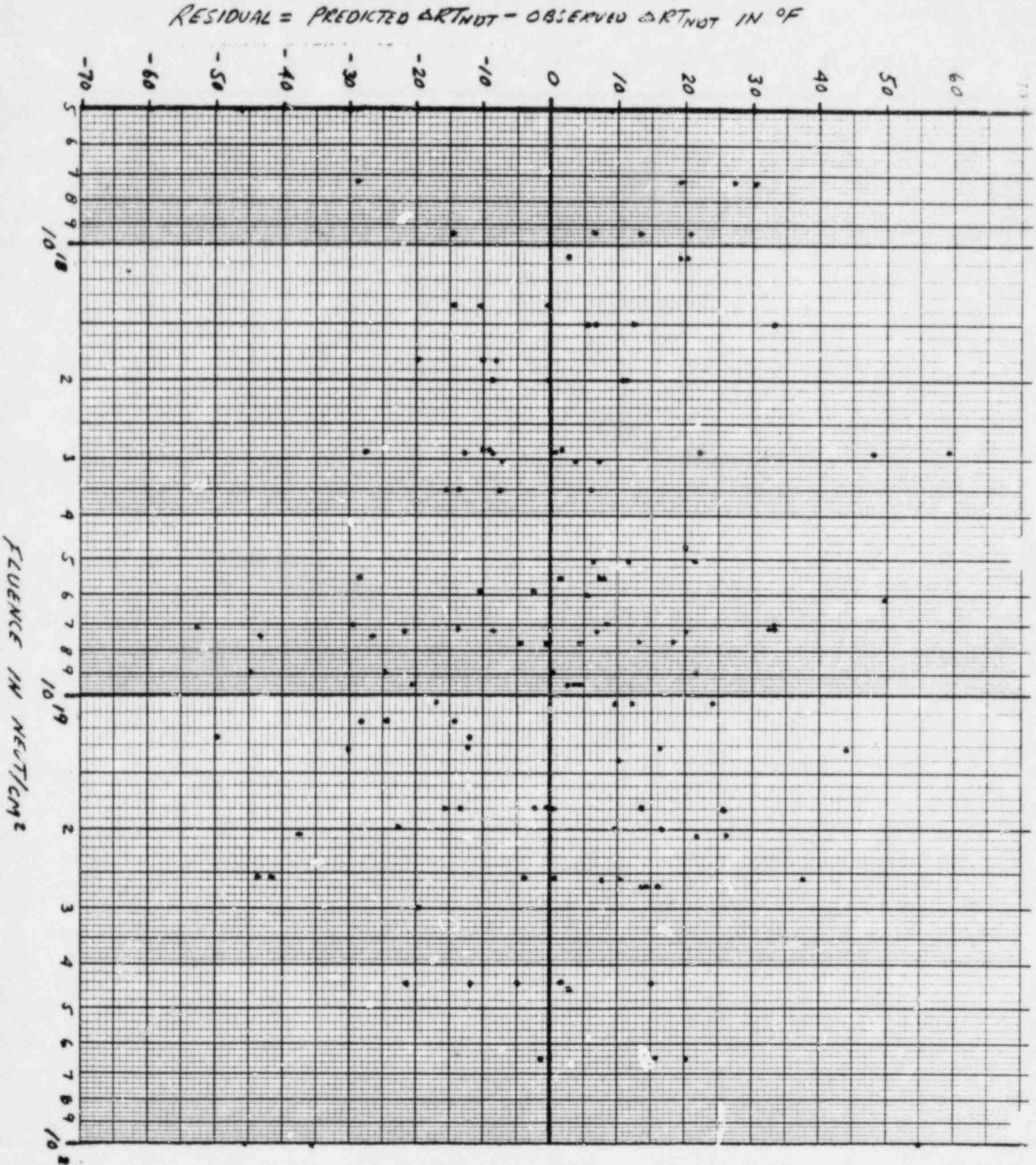

FIGURE H-2

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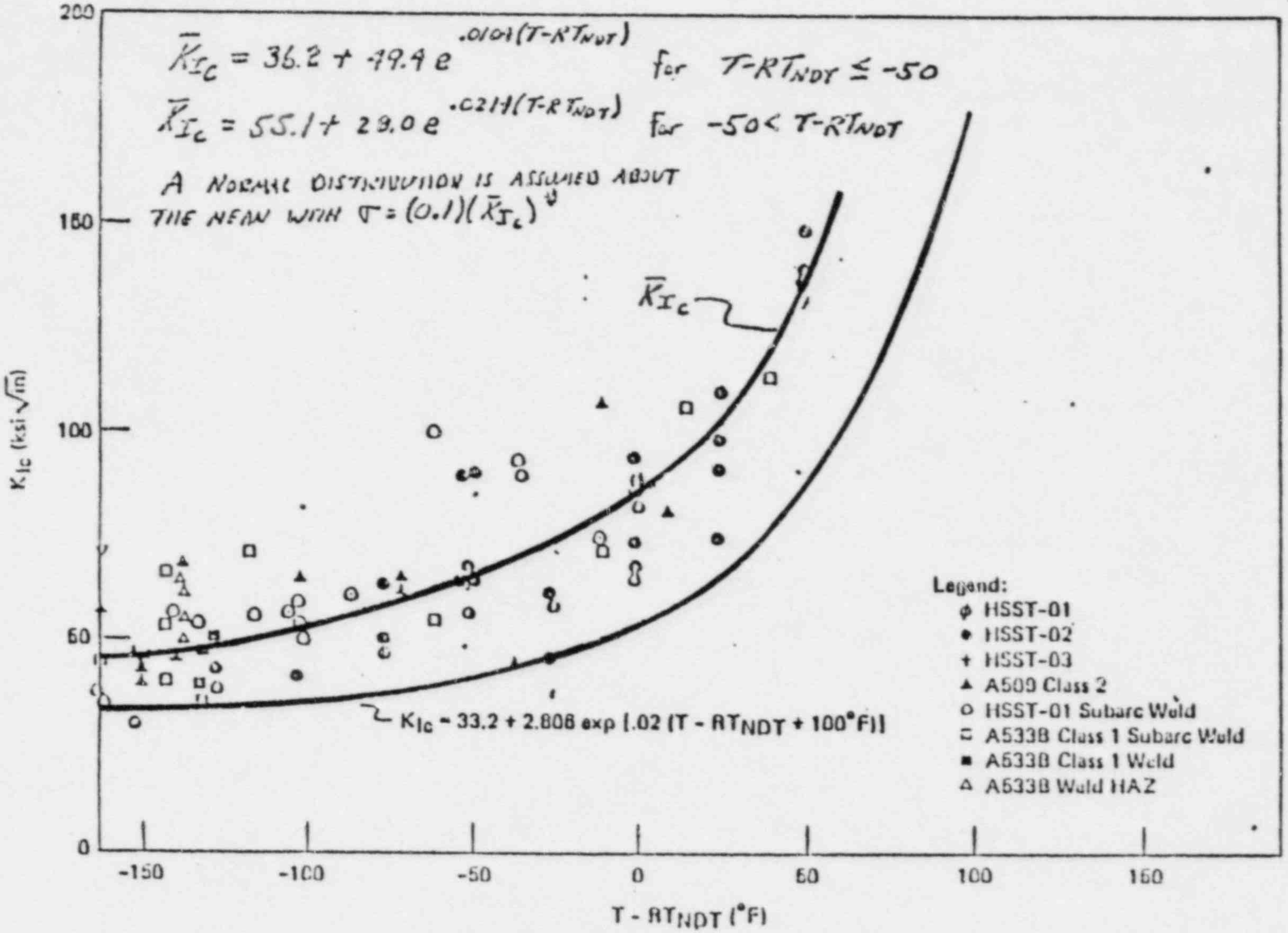


FIGURE H-3: K_{Ic} Reference Toughness Curve with Supporting Data

† SEE A.B. LIDIARD, "A SIMPLIFIED ANALYSIS OF PRESSURE VESSEL RELIABILITY", J. Br. Nucl. Energy Soc., July, 1971.

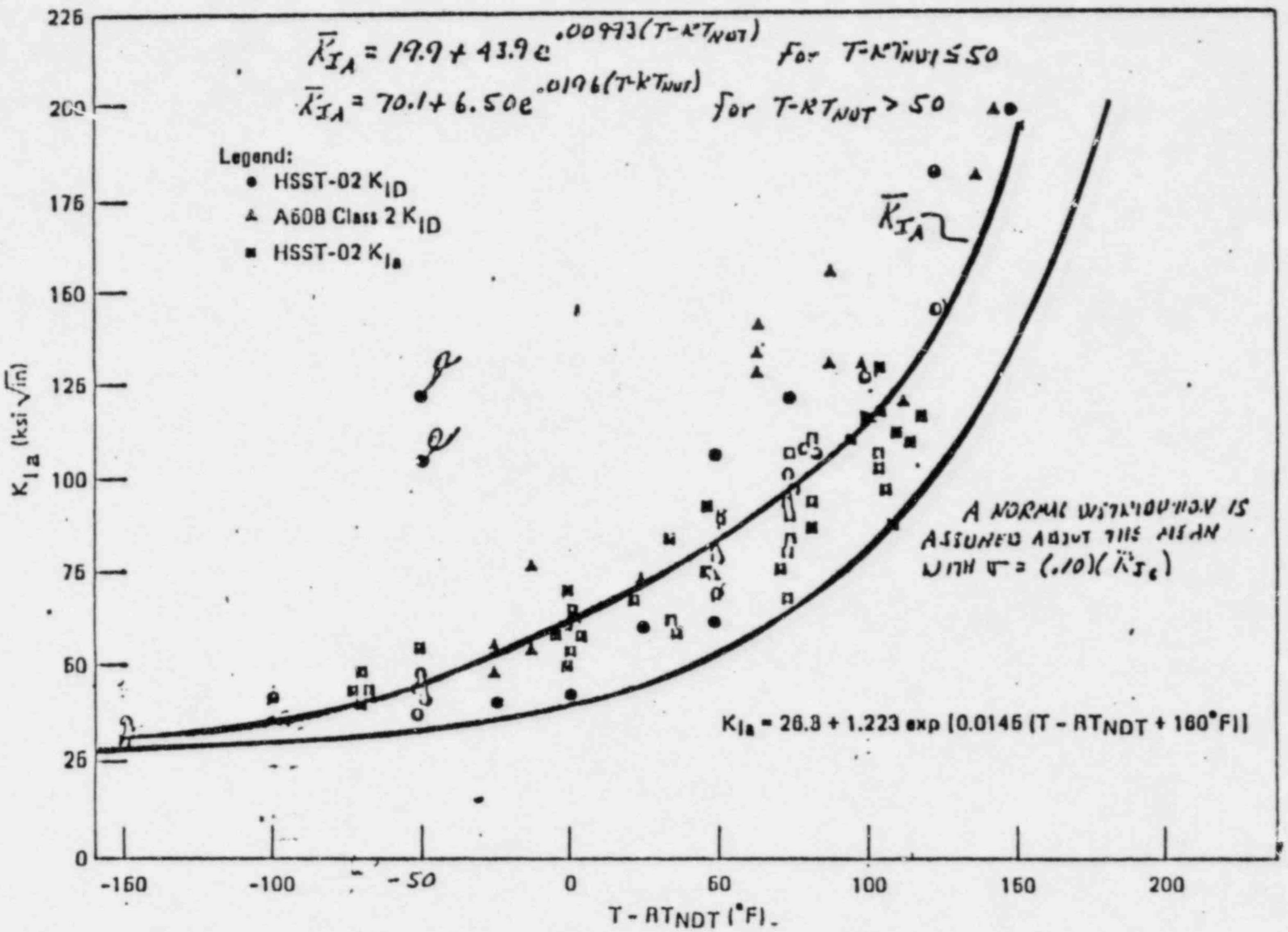
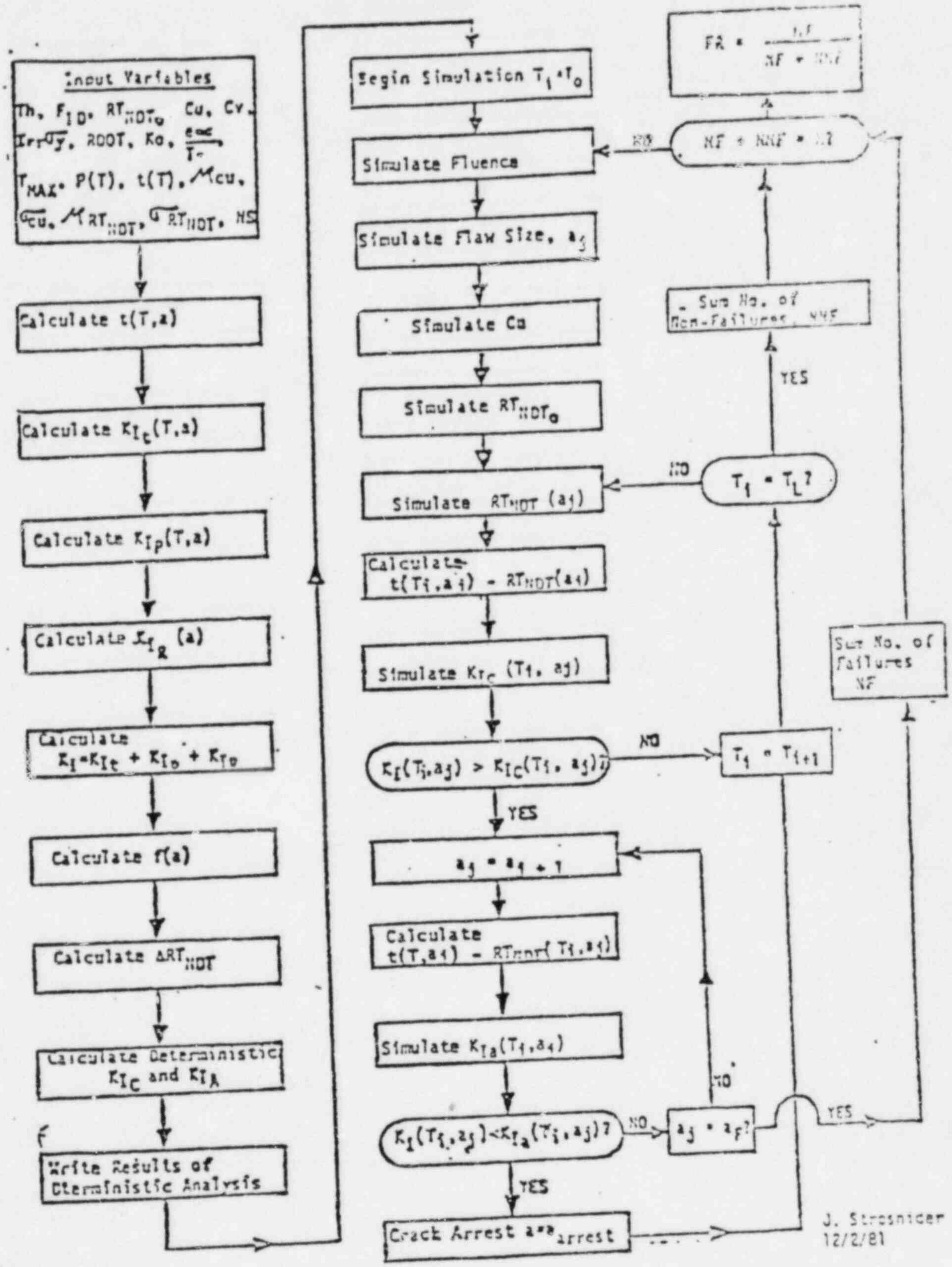


FIGURE H-4: K_{Ia} Reference Toughness Curve With Supporting Data



Input Variables
Calculate t(T,a)
Calculate KIt(T,a)
Calculate KIp(T,a)
Calculate KIR (a)
Calculate KI=KIt + KIp + KIR
Calculate f(a)
Calculate ΔRTNDT
Calculate Deterministic KIC and KIA
Write Results of Deterministic Analysis
Begin Simulation Ti=To
Simulate Fluence
Simulate Flaw Size, aj
Simulate Cu
Simulate RTNDTo
Simulate RTNDT (aj)
Simulate KIC (Ti, aj)
YES
NO
Simulate KIa(Ti,ai)
YES
NO
Crack Arrest a=aarrest
Ti = Ti+1
Ti = TL?
NO
YES
Sum No. of Non-Failures, NNF
NO
Sum No. of Failures NF
YES
NO
J. Strosnider
12/2/81

FIGURE H-5

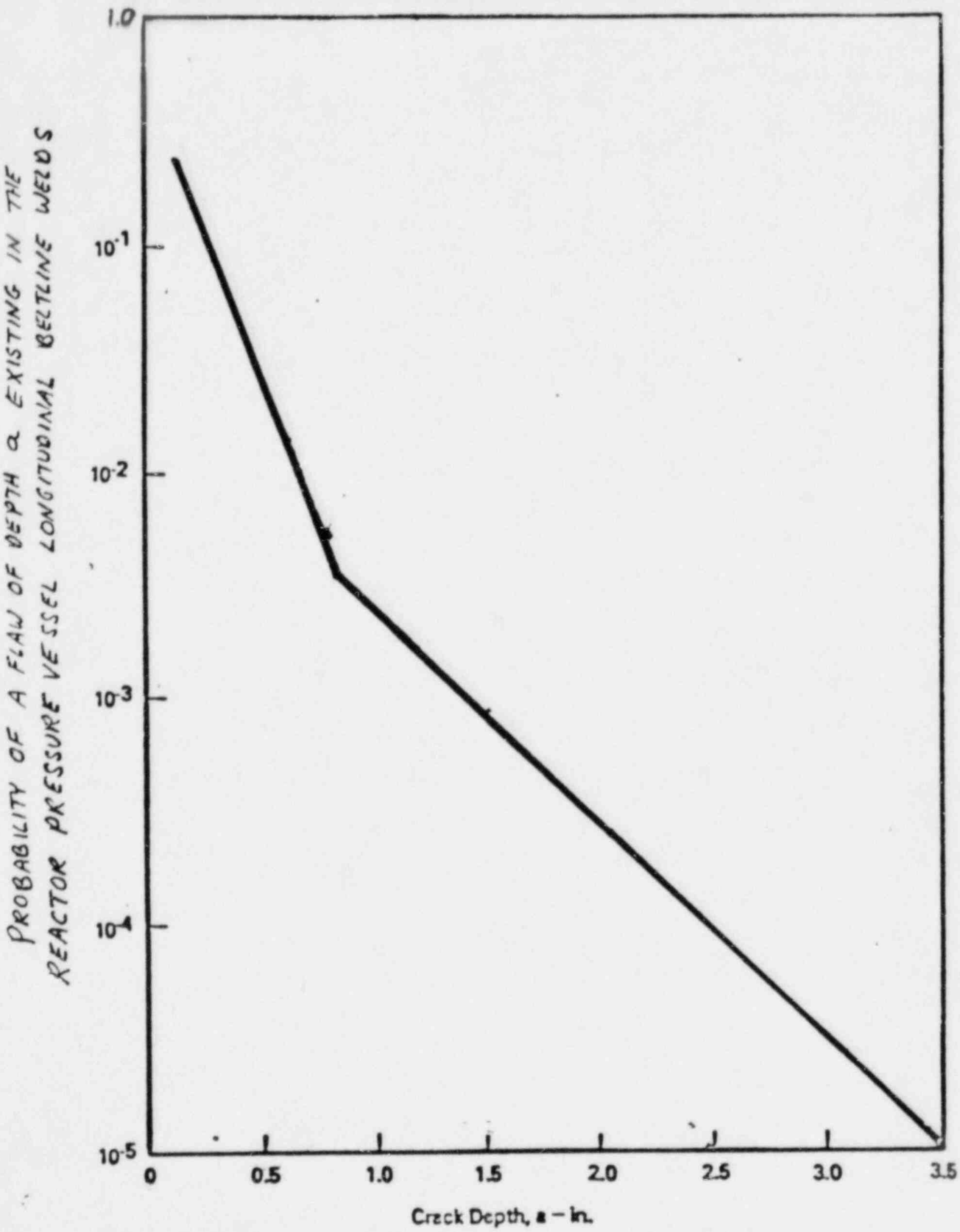


FIGURE H-6 : Reactor-Vessel-Beltline Flaw Distributions

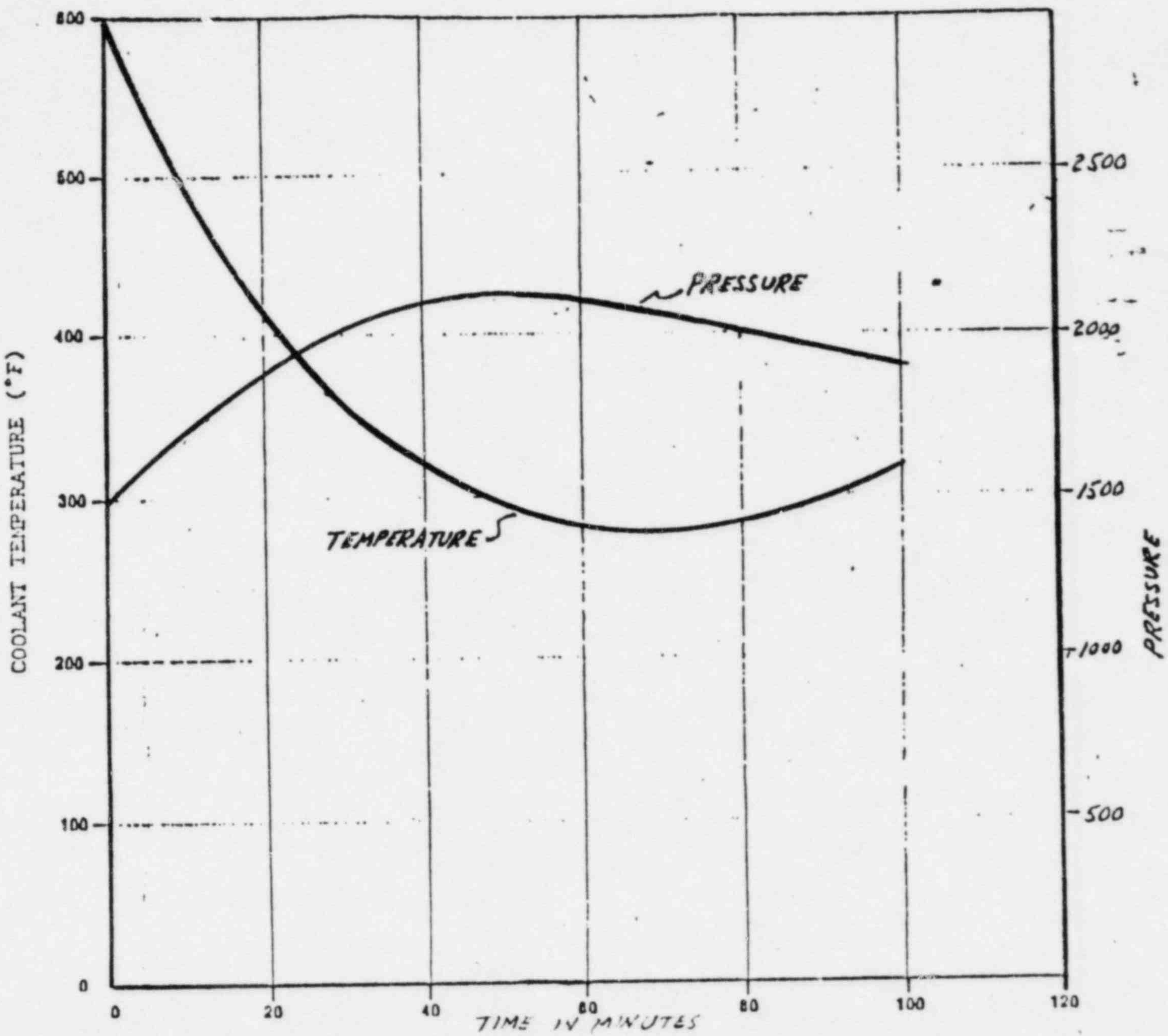


FIGURE H-7: RANCHO SECO TRANSIENT IDEALIZED
PRESSURE AND TEMPERATURE TIME HISTORIES

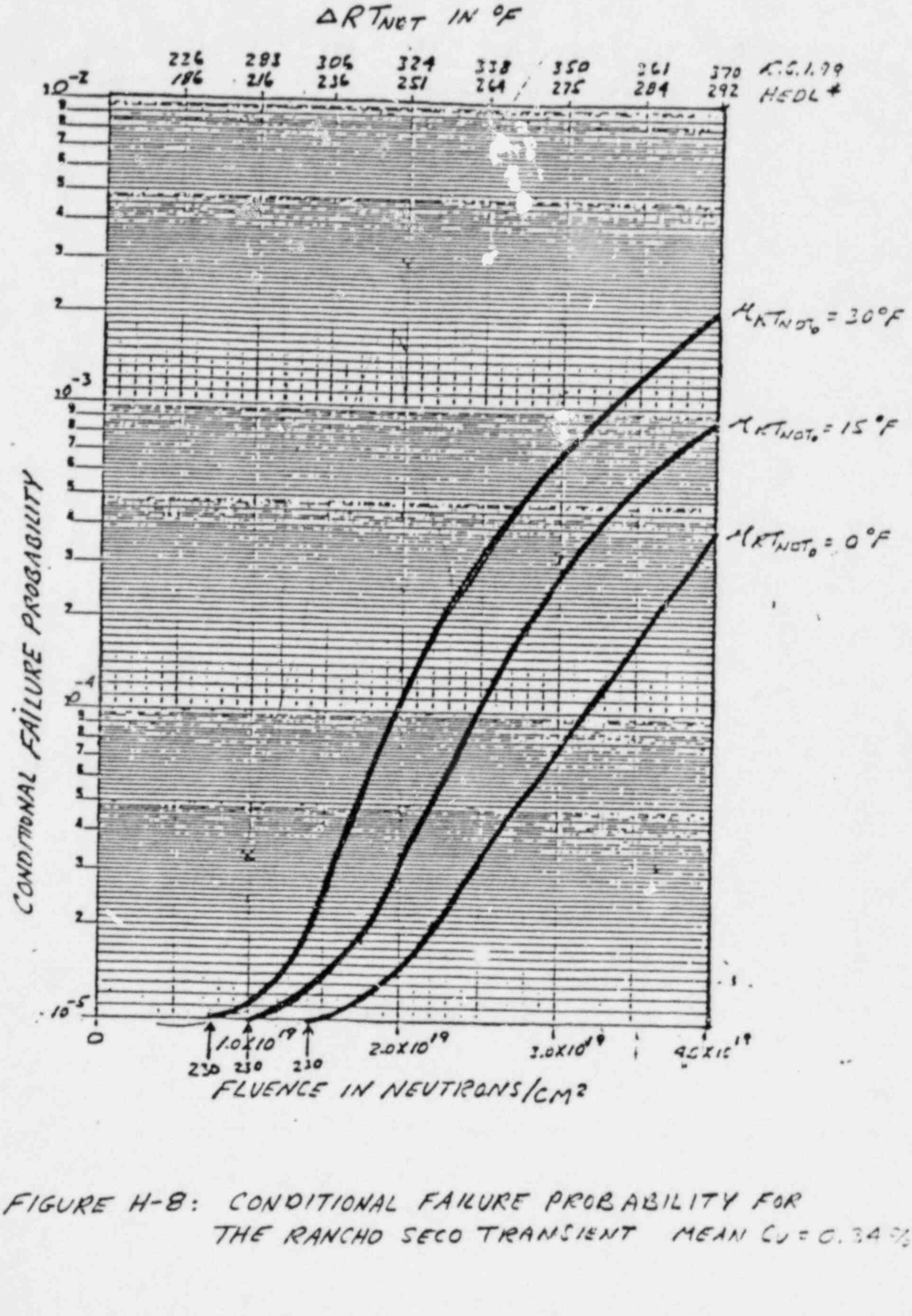


FIGURE H-8: CONDITIONAL FAILURE PROBABILITY FOR THE RANCHO SECO TRANSIENT MEAN Cu = 0.34%

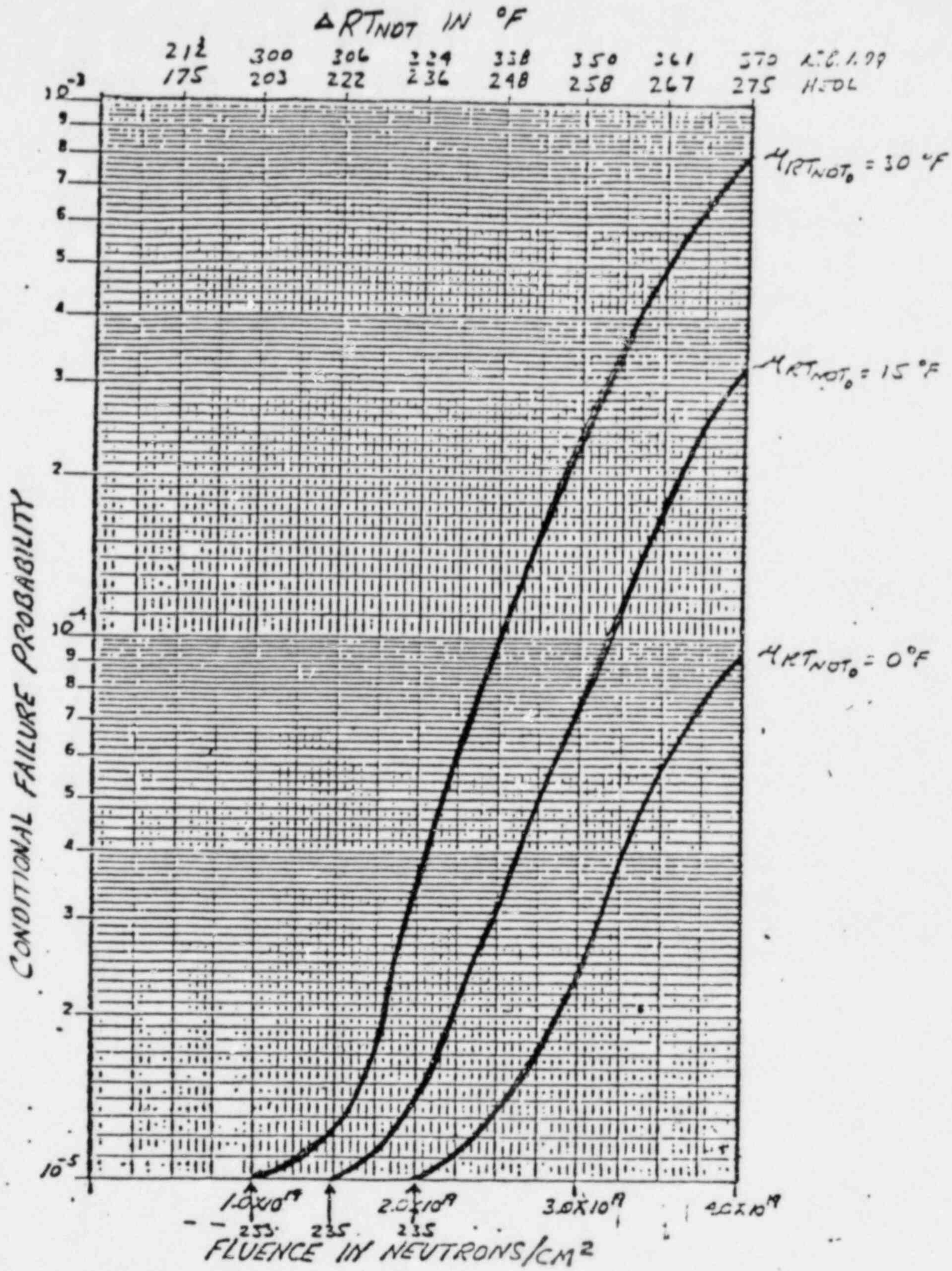


FIGURE H-9: CONDITIONAL FAILURE PROBABILITY FOR THE RANCHO SECO TRANSIENT MEAN Cu = 0.32%

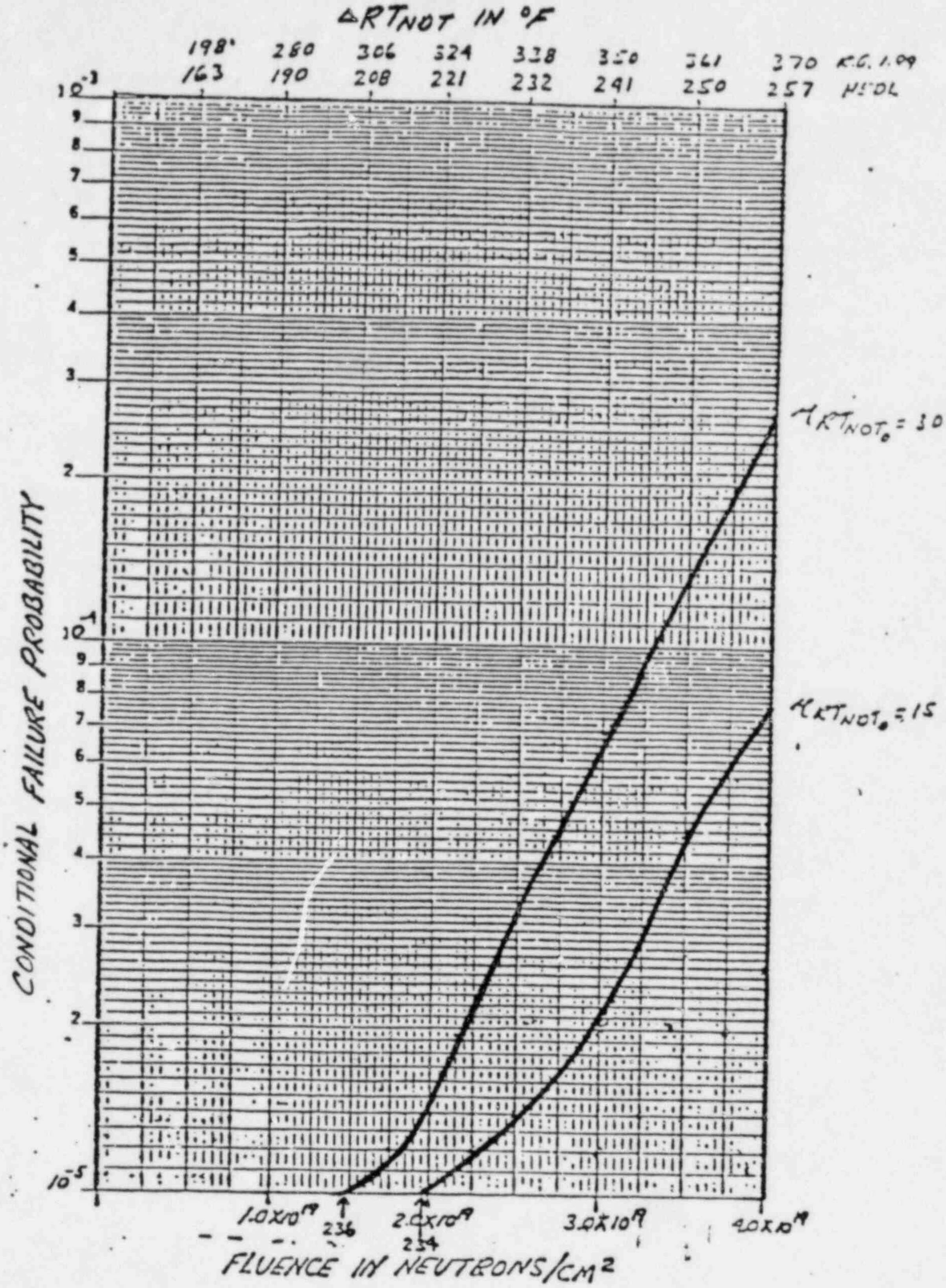


FIGURE H-10: CONDITIONAL FAILURE PROBABILITY FOR THE RANCHO SECO TRANSIENT MEAN Cu = 0.30%

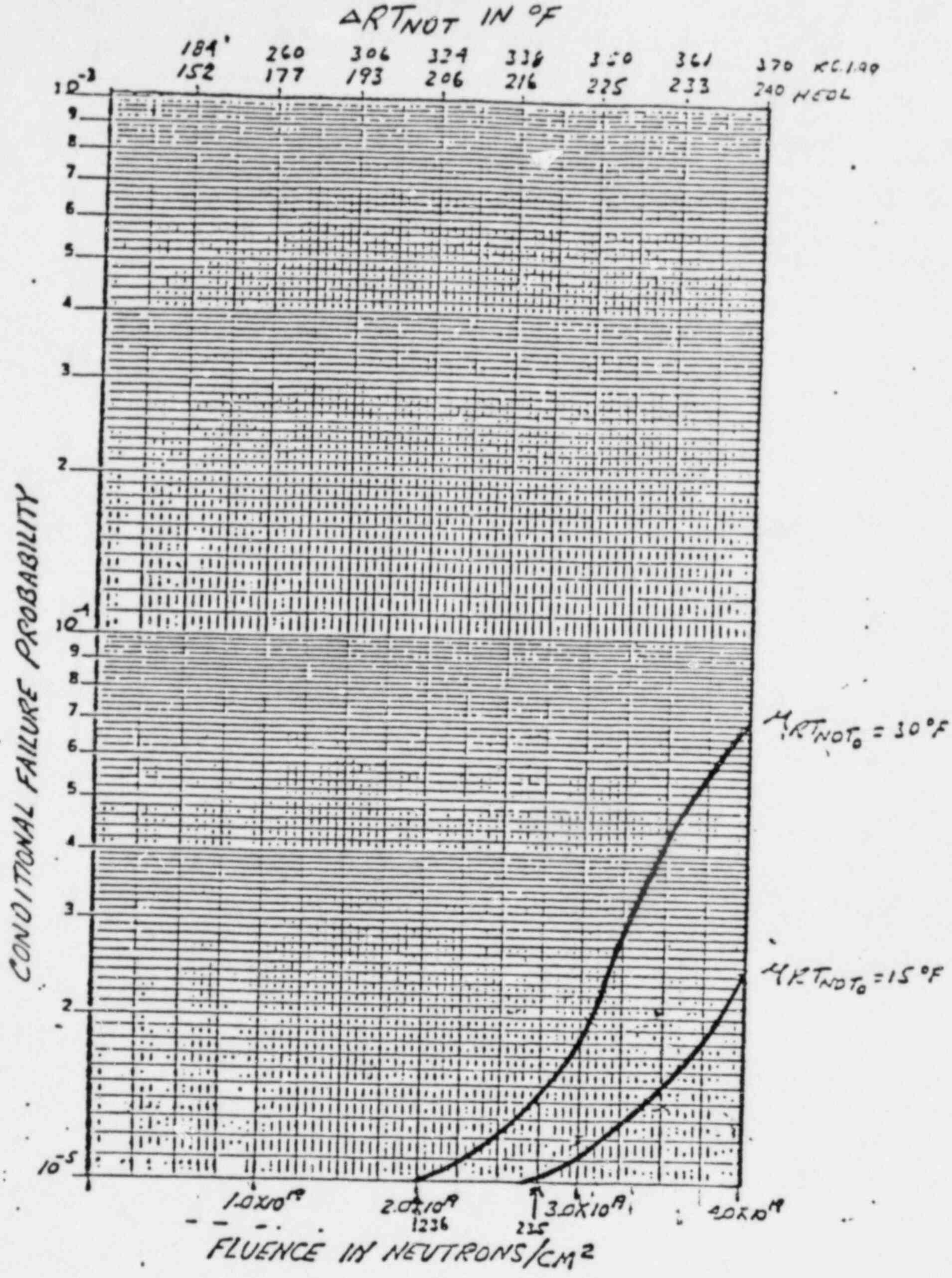


FIGURE H-11: CONDITIONAL FAILURE PROBABILITY FOR THE RANCHO SECO TRANSIENT MEAN Cu = 0.28%

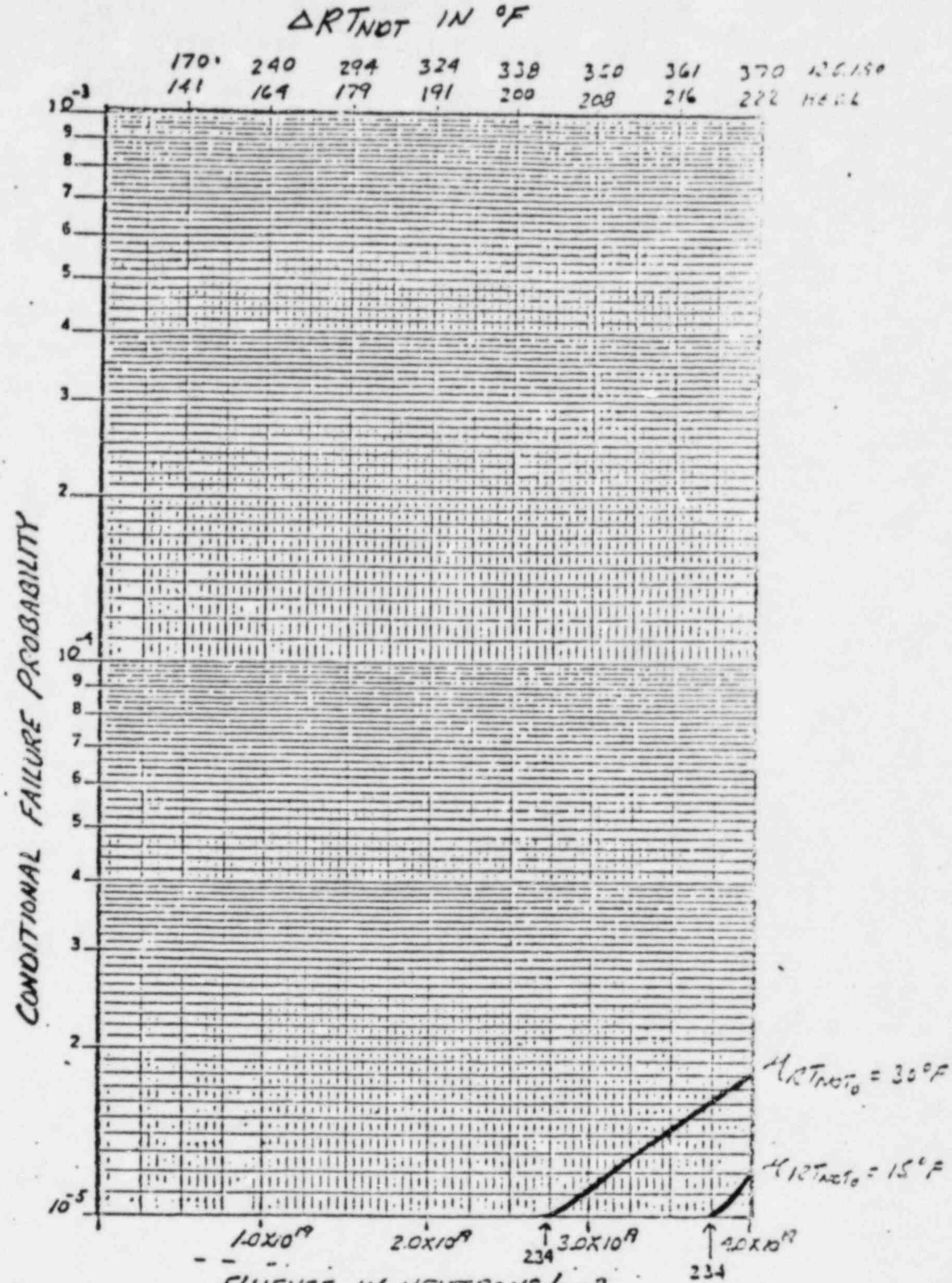


FIGURE H-12: CONDITIONAL FAILURE PROBABILITY FOR THE RANCHO SECO TRANSIENT MEAN Cu = 0.26%

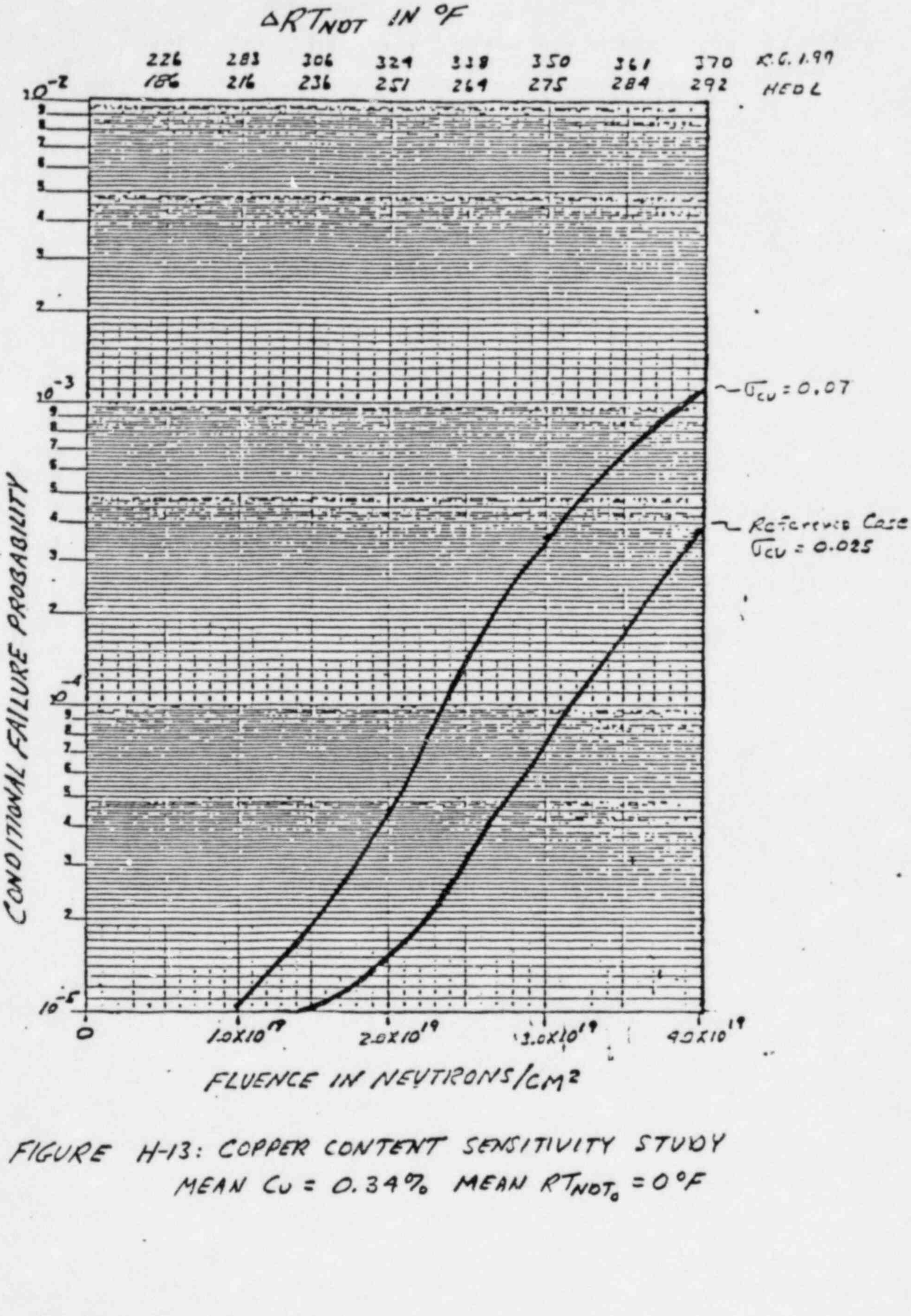


FIGURE H-13: COPPER CONTENT SENSITIVITY STUDY

MEAN $Cu = 0.34\%$ MEAN $RT_{NOT_0} = 0°F$

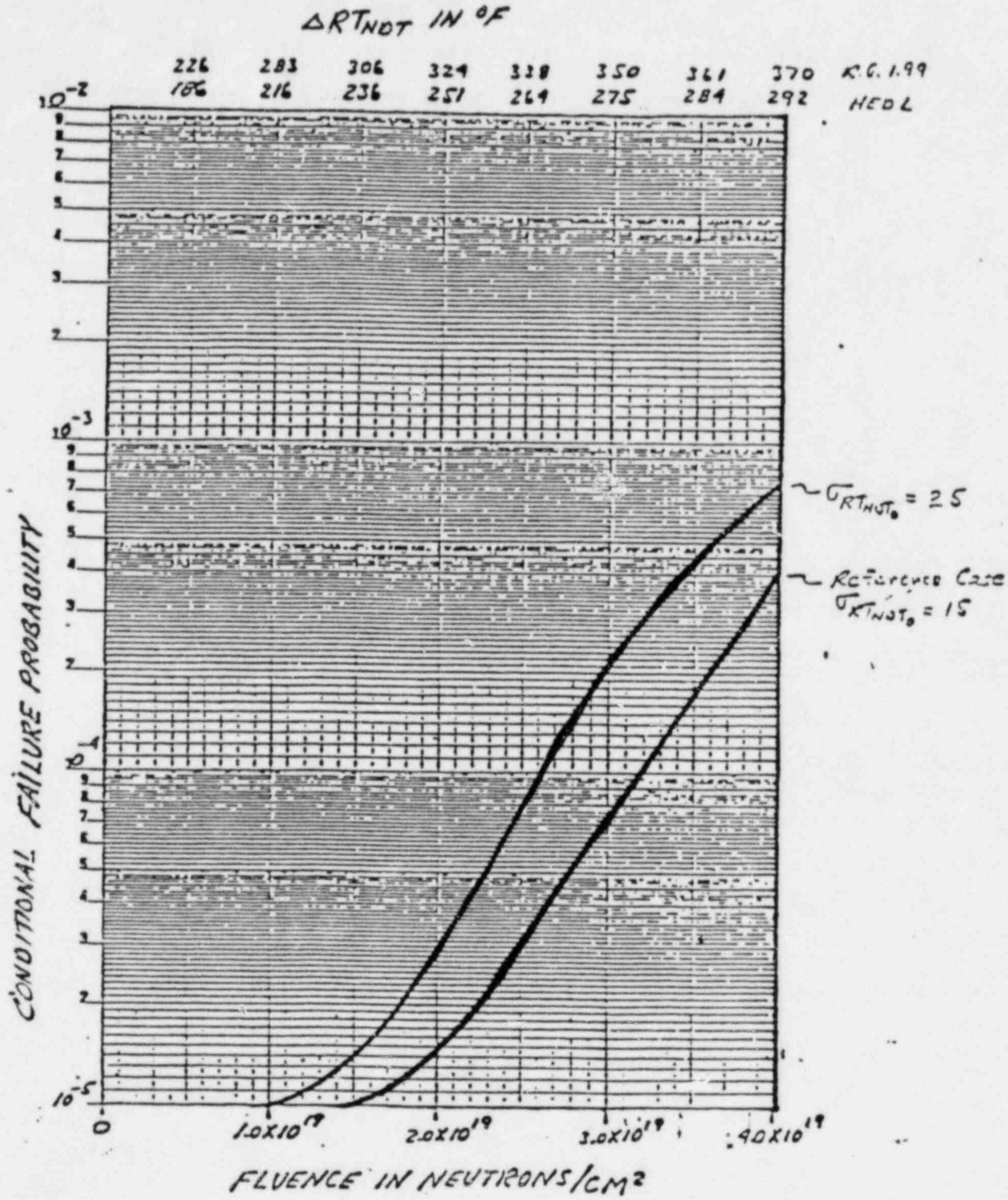


FIGURE H-14: INITIAL RT_{NDT} SENSITIVITY STUDY

MEAN Cu = 0.34% MEAN RT_{NDT_0} = 0°F

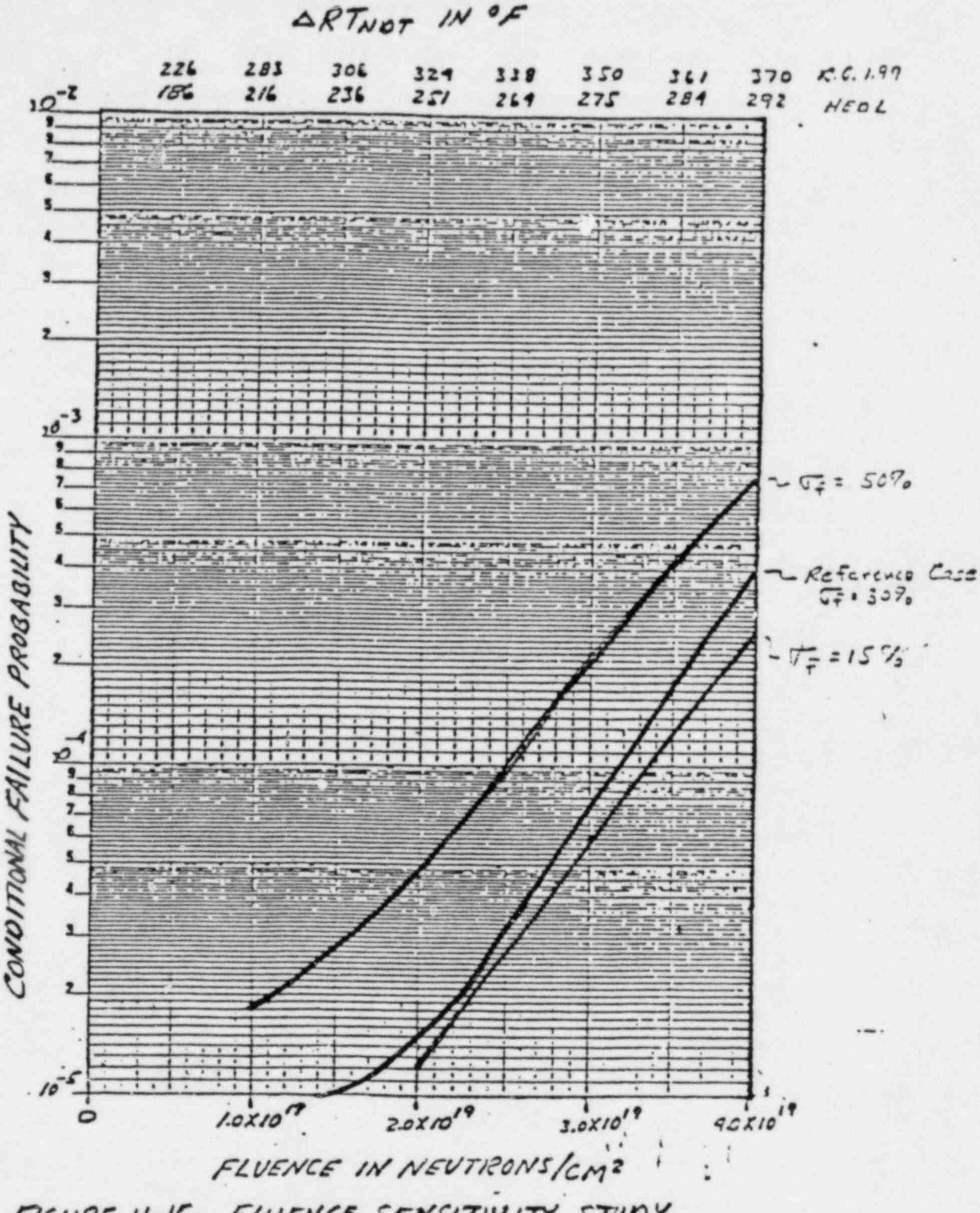


FIGURE H-15: FLUENCE SENSITIVITY STUDY

MEAN Cu = 0.34% MEAN RT_{NDT_0} = 0°F

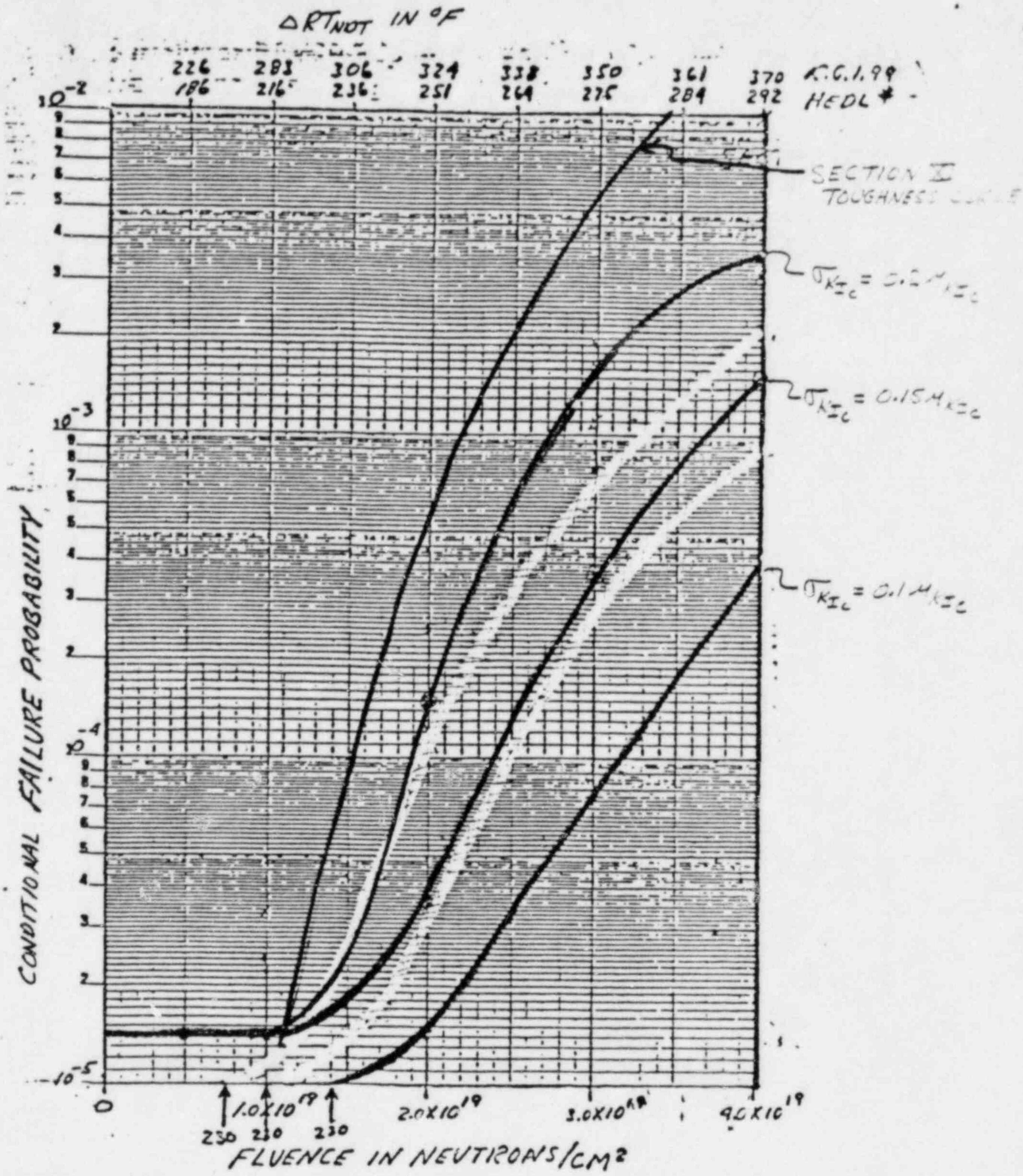


FIGURE H-16: FRACTURE TOUGHNESS DISTRIBUTION SENSITIVITY STUDY MEAN Cu = 0.34%

MEAN RT_{NDT_0} = 0°F

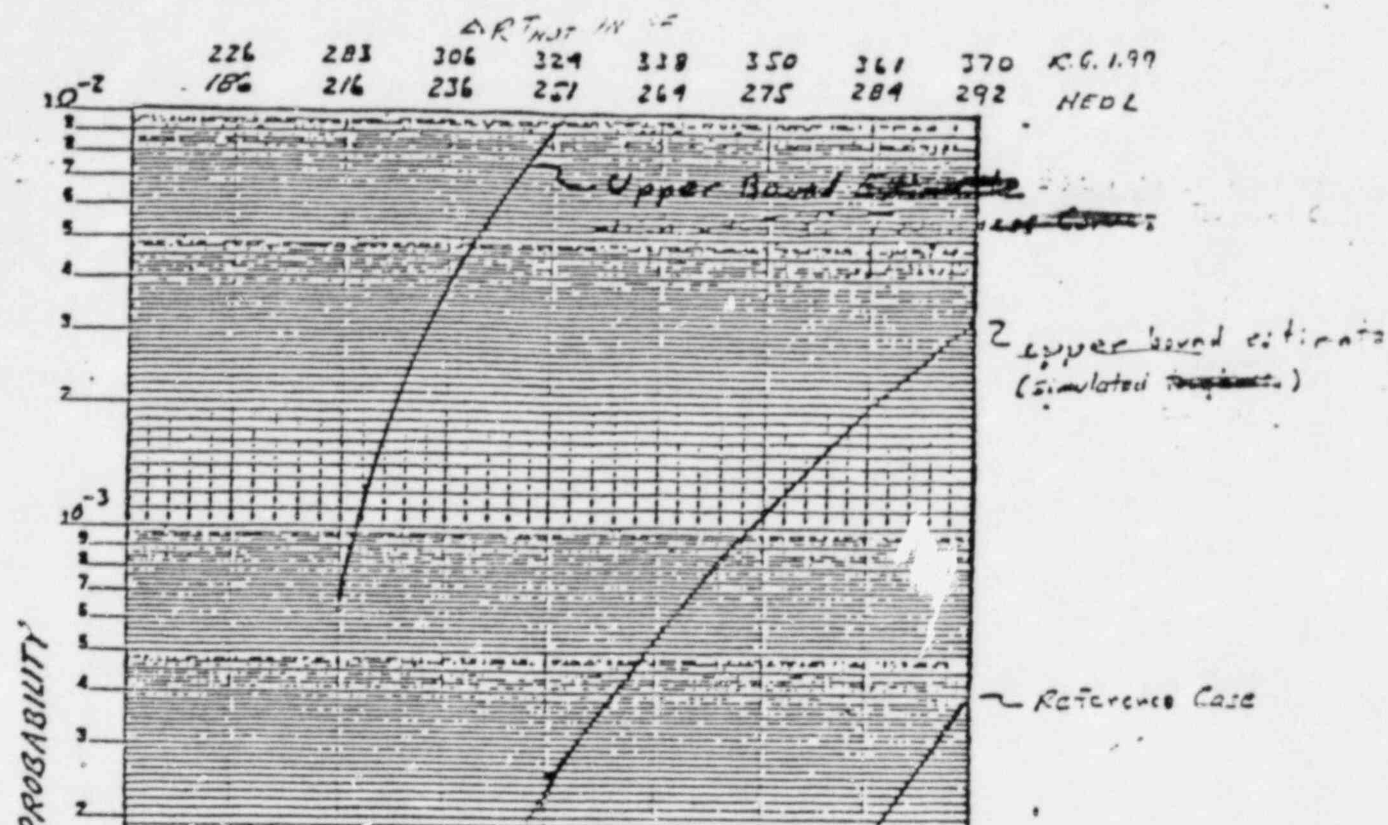


FIGURE H-17: SIMULTANEOUS INCREASE IN THE VARIABILITY OF THE RANDOM VARIABLES
MEAN Cu = 0.34% MEAN RT_{NDT_0} = 0°F

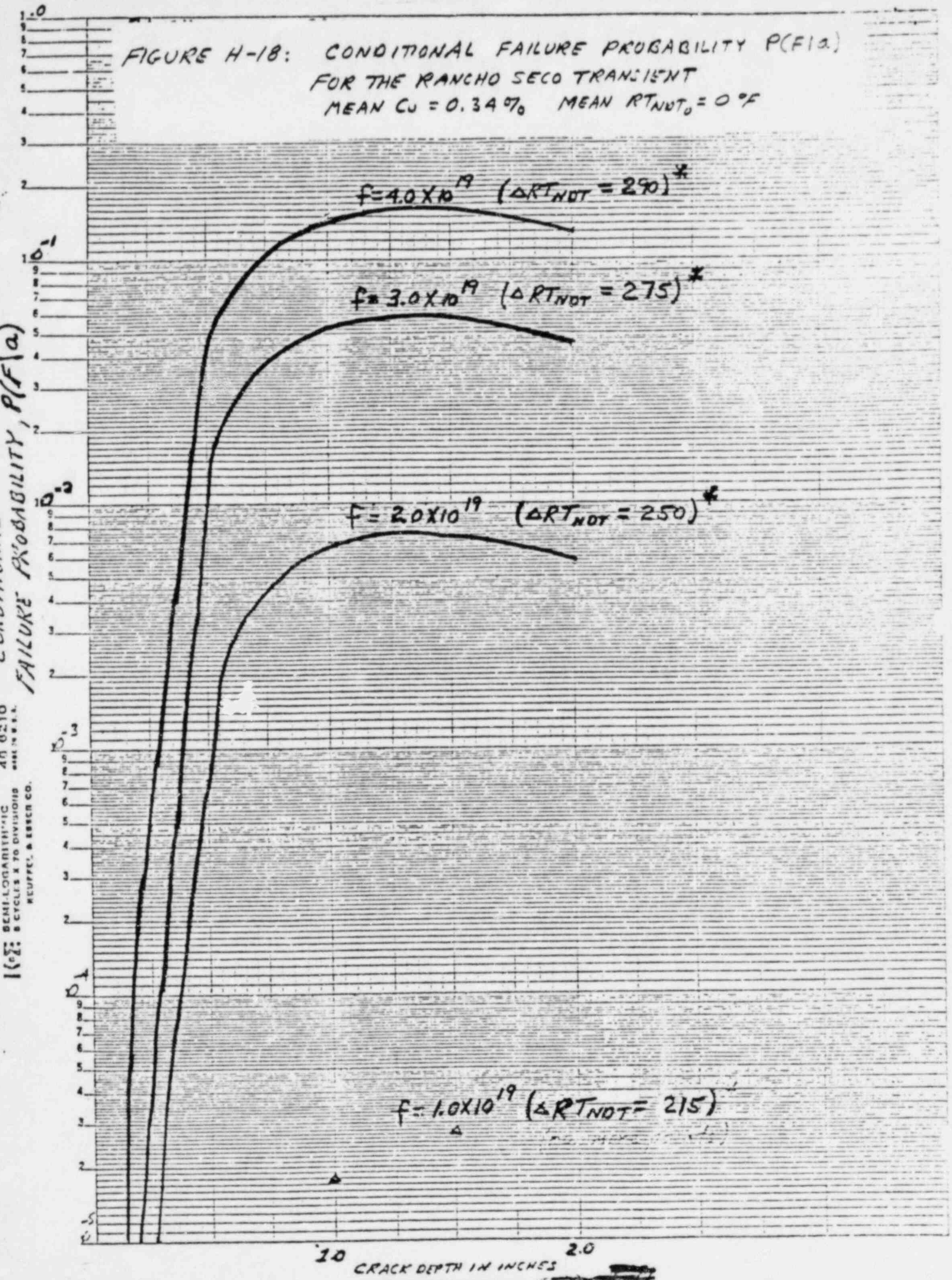


FIGURE H-18: CONDITIONAL FAILURE PROBABILITY P(F|a) FOR THE RANCHO SECO TRANSIENT MEAN Cu = 0.34% MEAN RT_{NDT_0} = 0°F

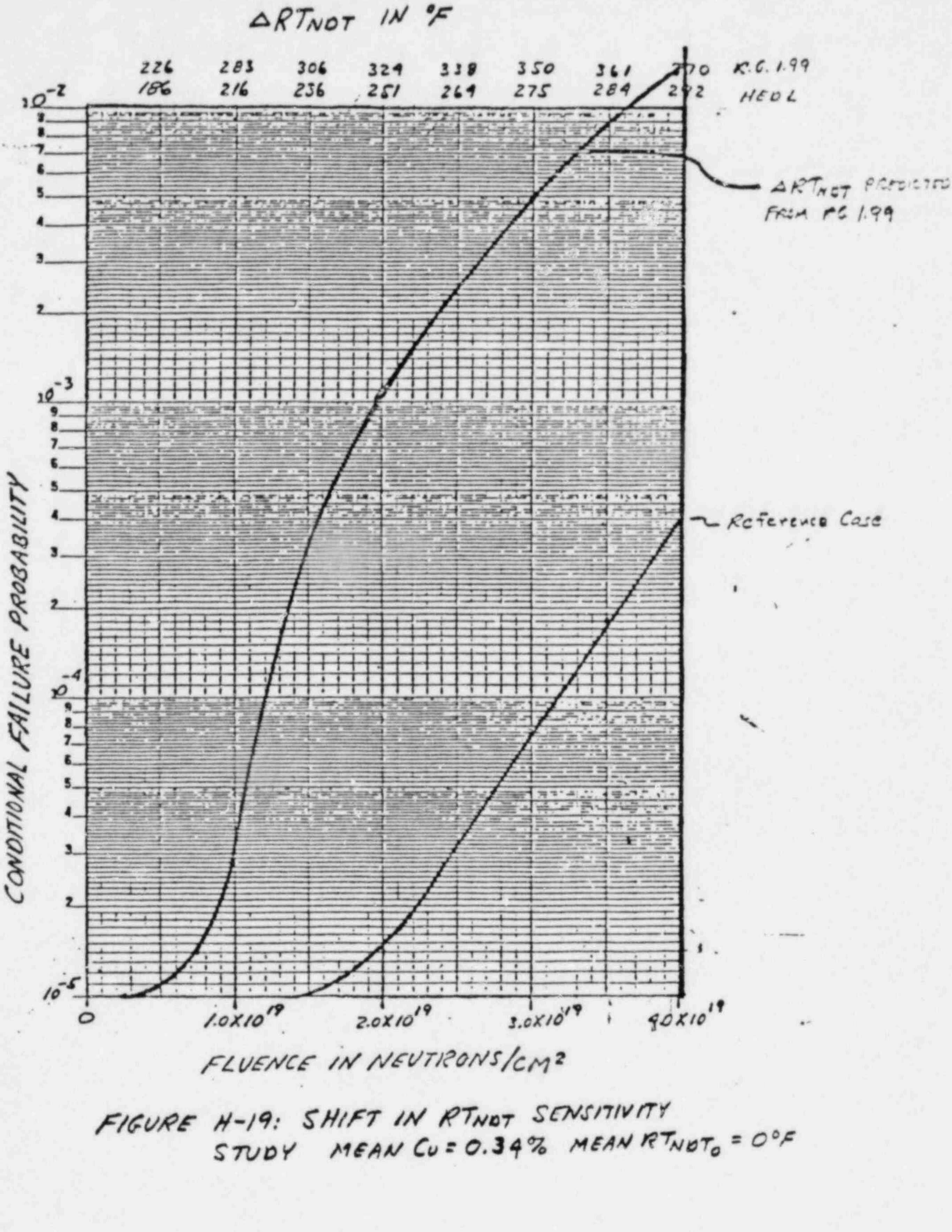


FIGURE H-19: SHIFT IN RT_{NDT} SENSITIVITY STUDY MEAN Cu = 0.34% MEAN RT_{NDT_0} = 0°F

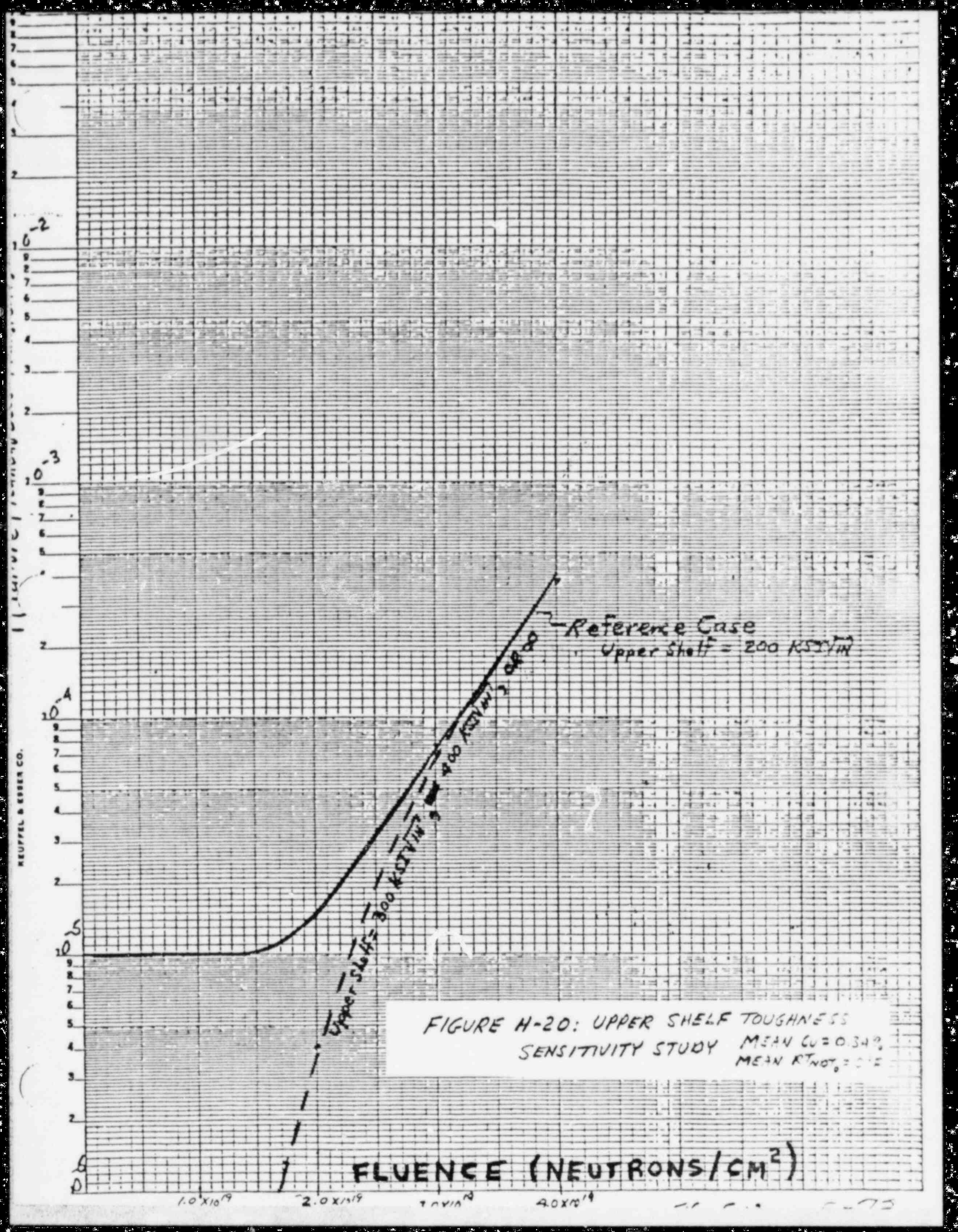


FIGURE H-20: UPPER SHELF TOUGHNESS SENSITIVITY STUDY MEAN Cu = 0.34%, MEAN RT_{NDT_0} = [illegible]

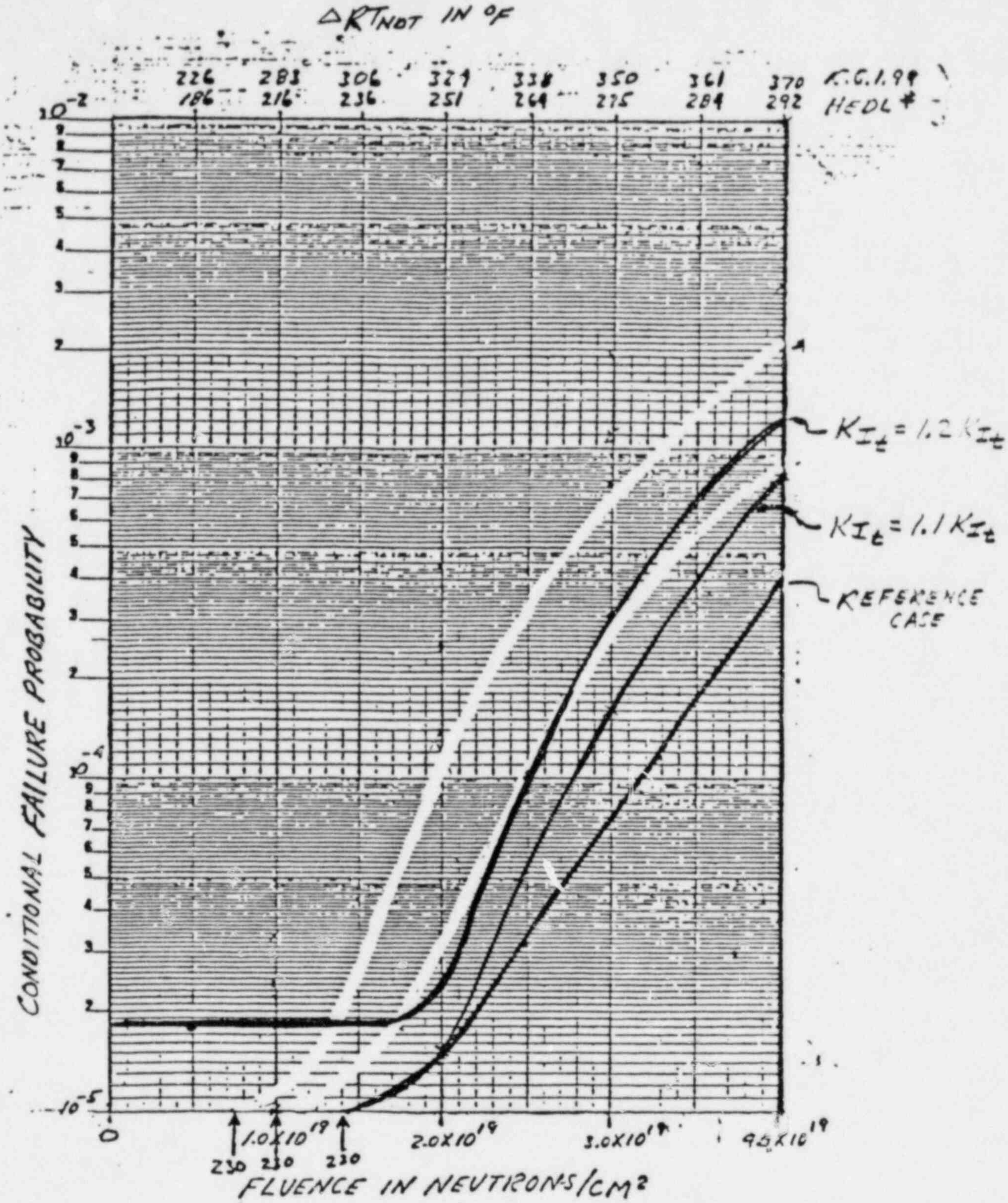


FIGURE H-21: CLADDING SENSITIVITY STUDY
MEAN Cu = 0.34 % MEAN RT_{NDT_0} = 0°F

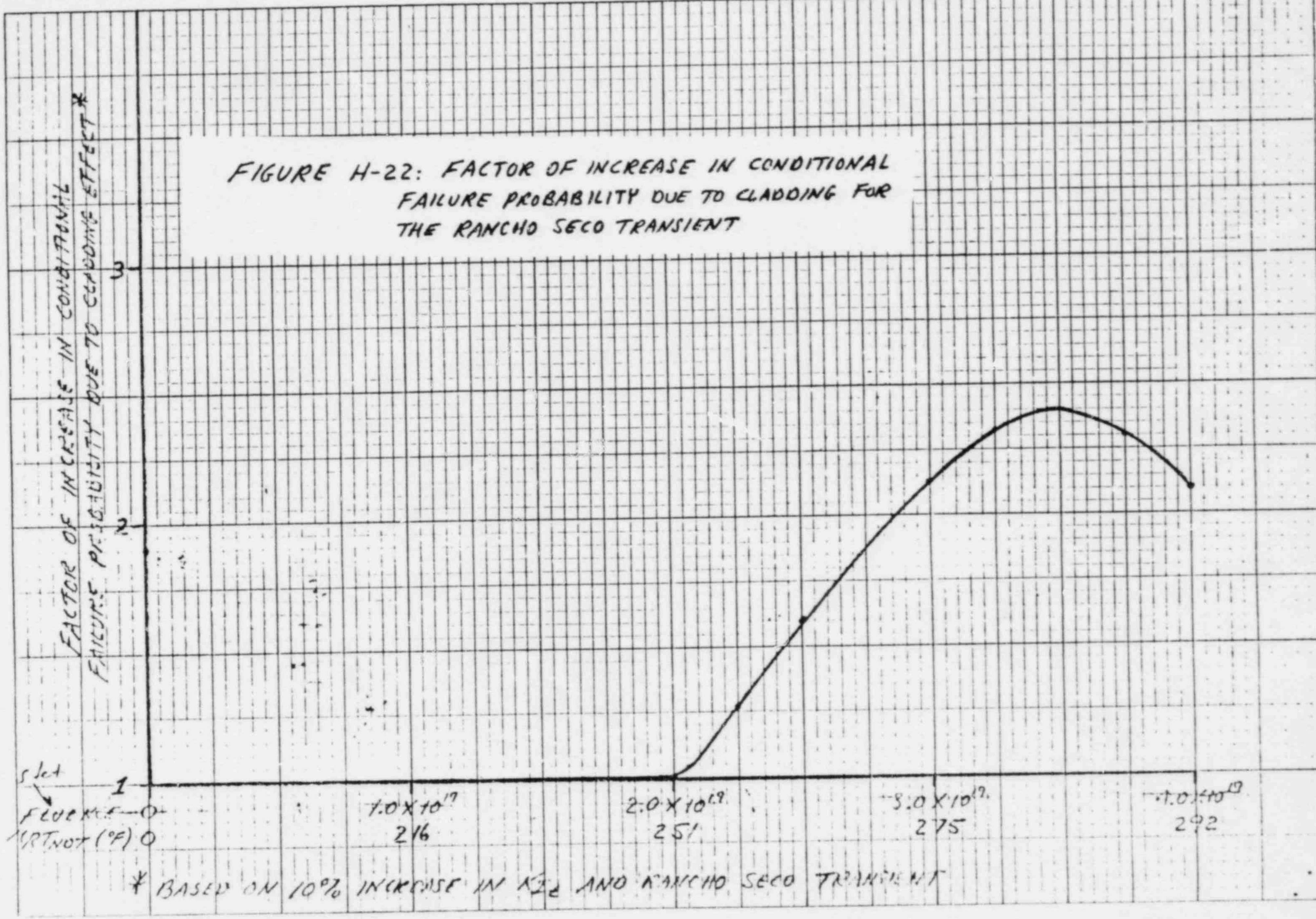


FIGURE H-22: FACTOR OF INCREASE IN CONDITIONAL FAILURE PROBABILITY DUE TO CLADDING FOR THE RANCHO SECO TRANSIENT

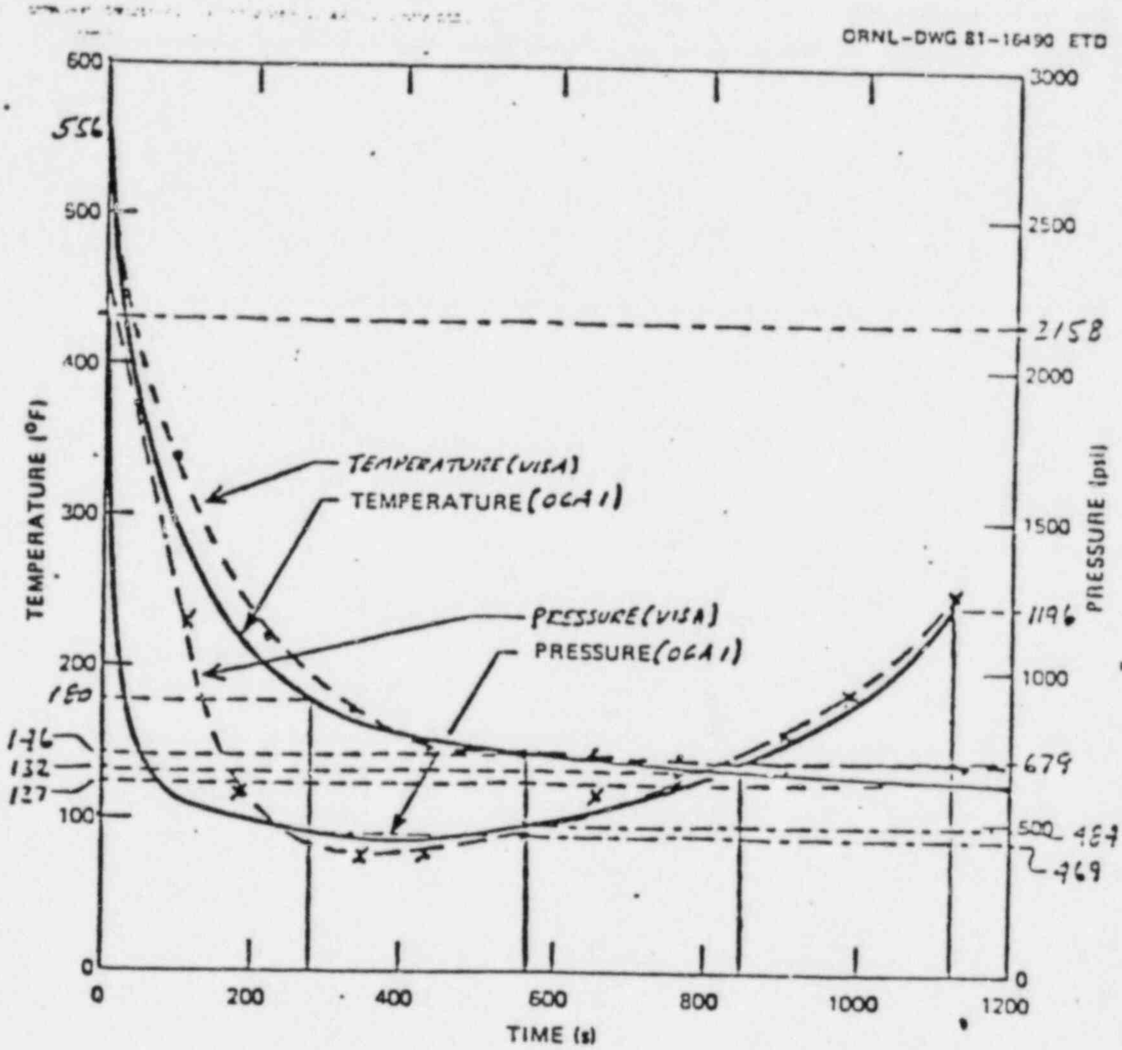


FIGURE H-23: I.R.T MAIN STEAM LINE BREAK PRESSURE AND TEMPERATURE TIME HISTORIES

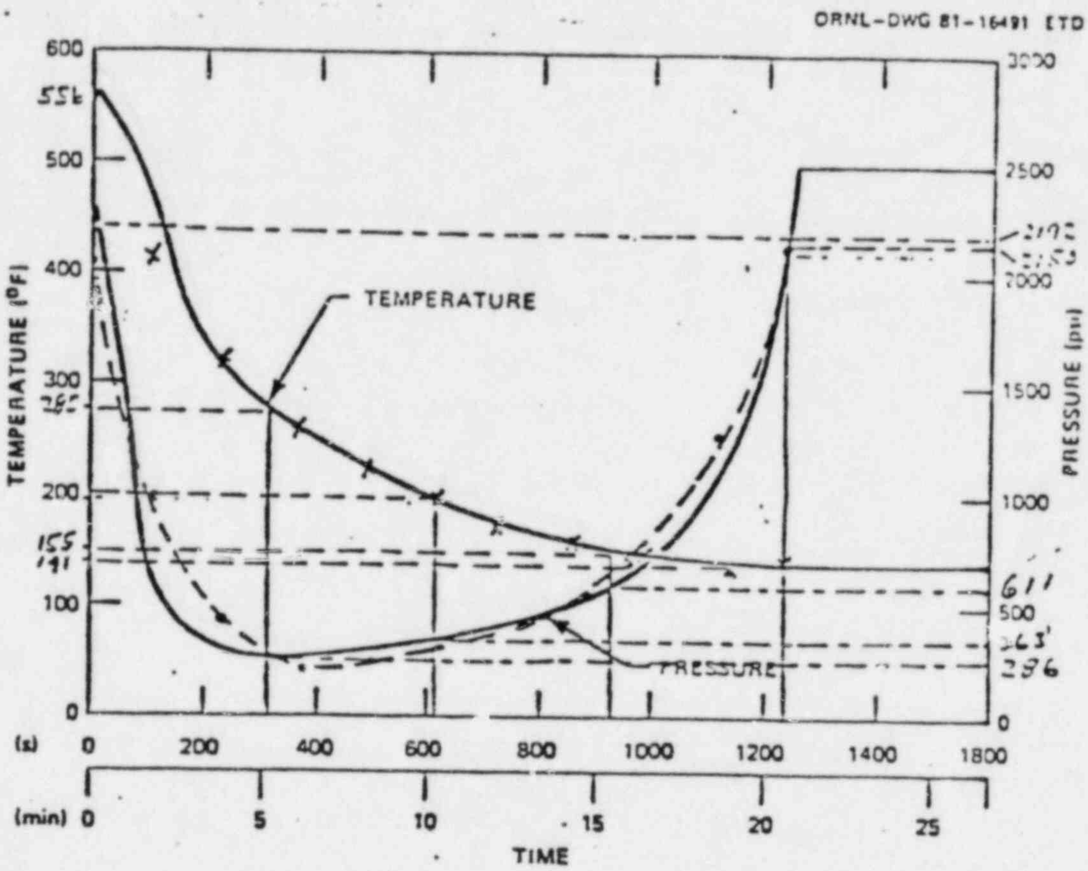


FIGURE H-24: TURBINE TRIP WITH STUCK OPEN BYPASS VALVES (SCRAM WORTH = 0.061 $\Delta k/k$) TEMPERATURE AND PRESSURE TIME HISTORIES

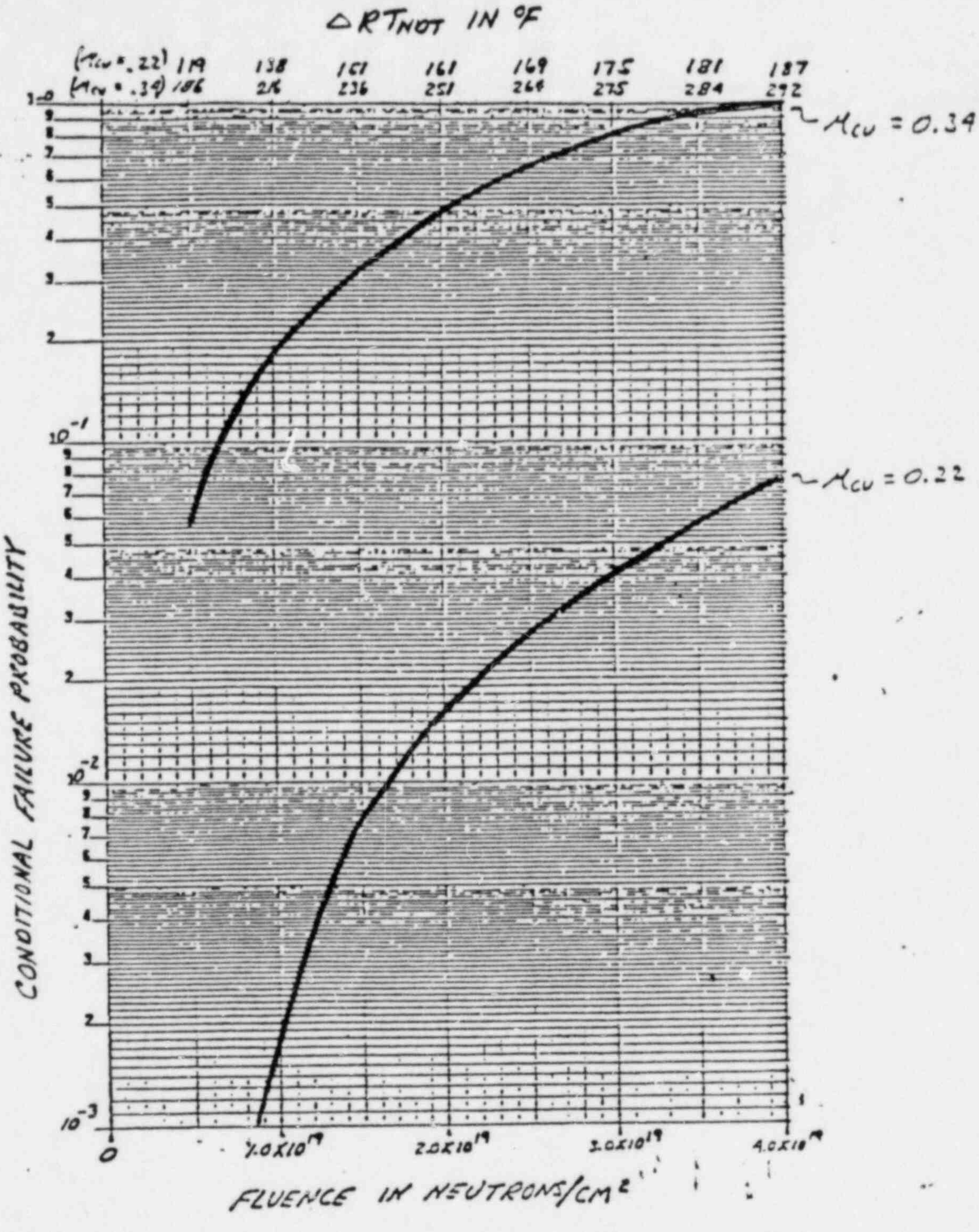


FIGURE H-25: CONDITIONAL FAILURE PROBABILITY FOR THE IRT MSLB MEAN RT_{NDT_0} = 0°F

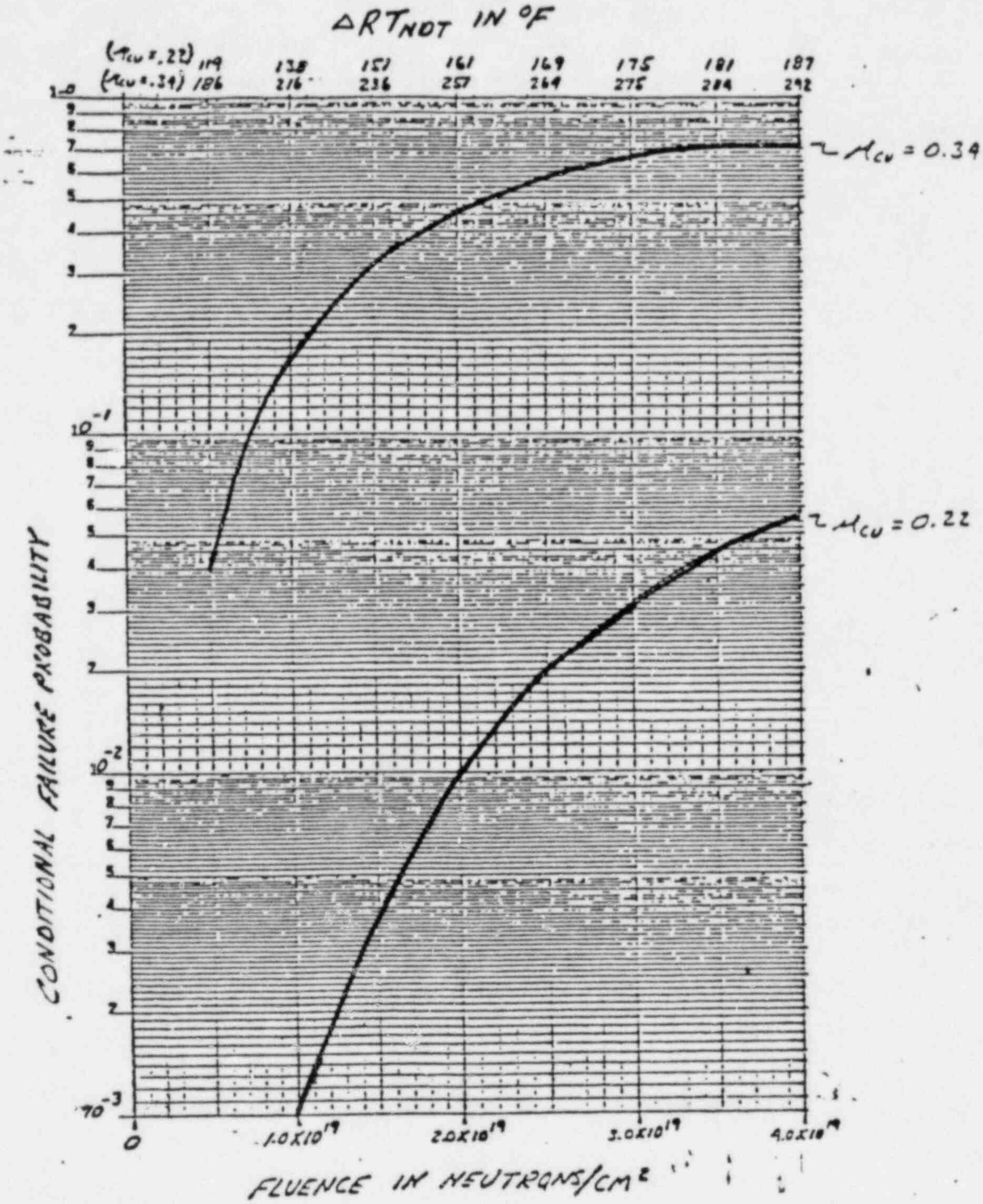


FIGURE H-26: CONDITIONAL FAILURE PROBABILITY FOR TURBINE TRIP/STUCK OPEN BYPASS VALVE MEAN RT_{NDT_0} = 0°F

FIGURE 5-17

P=1000

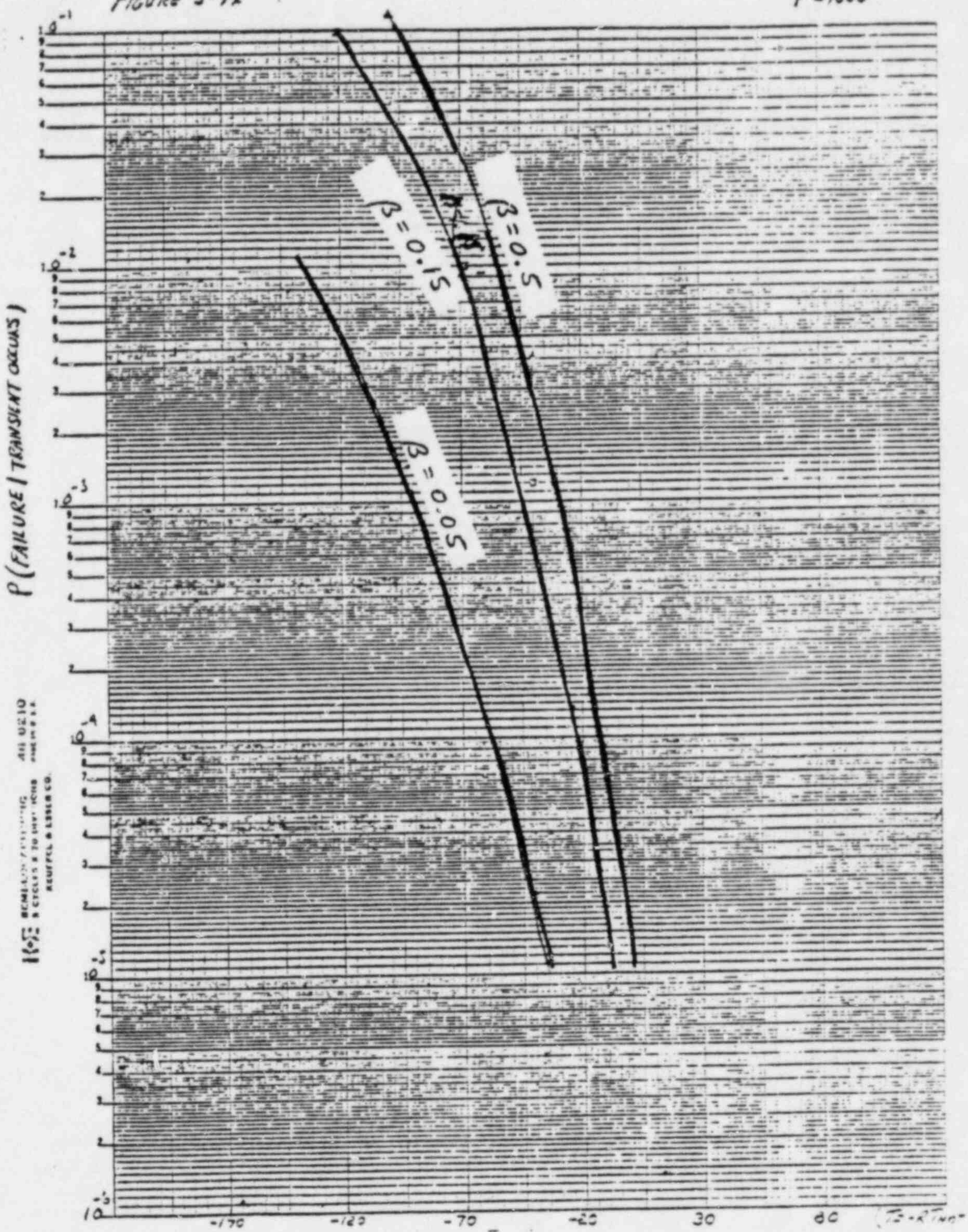


FIGURE H-27: SENSITIVITY OF CONDITIONAL FAILURE PROBABILITY TO TF-RTNDT

DRAFT

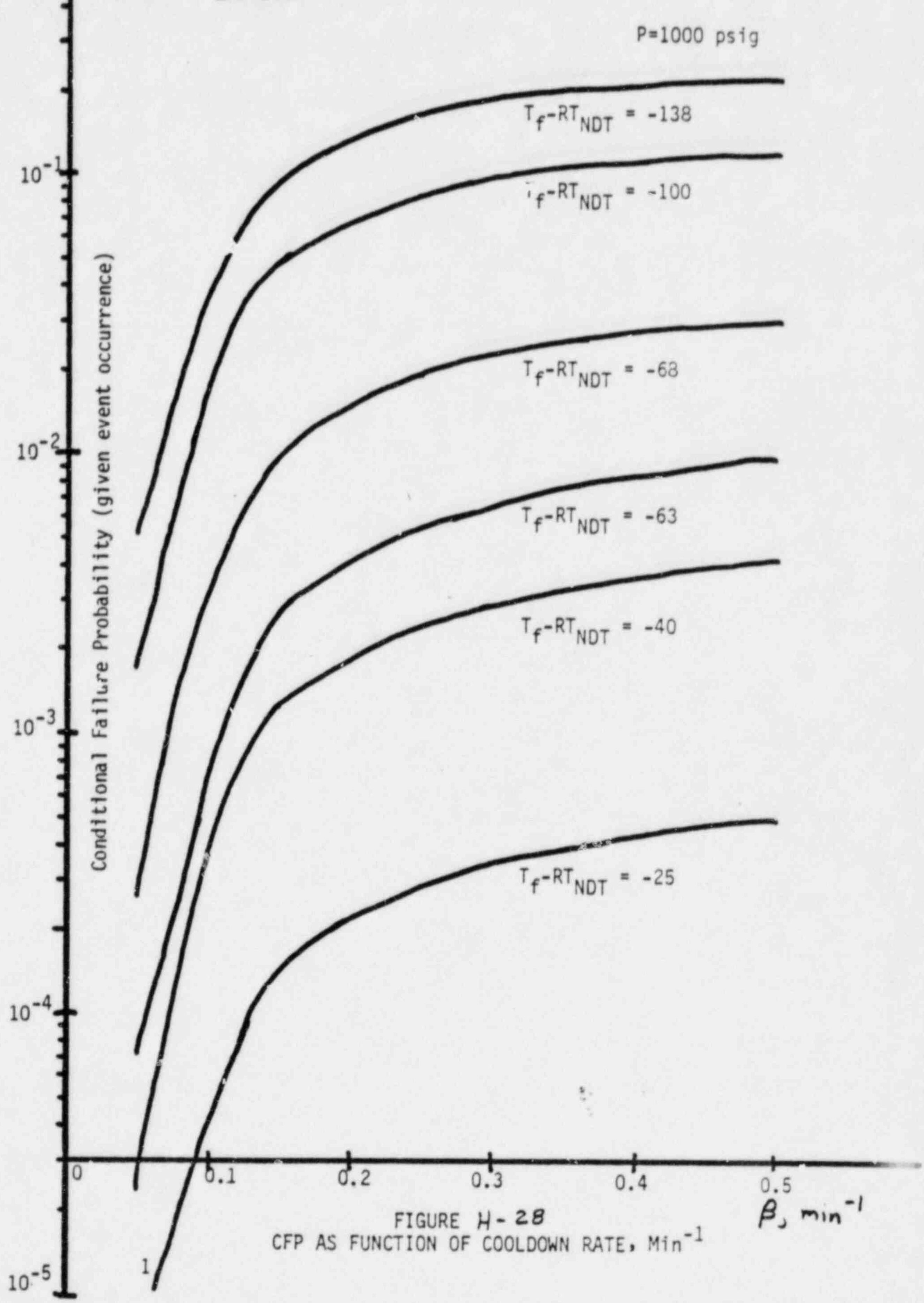


FIGURE H-28
CFP AS FUNCTION OF COOLDOWN RATE, Min^{-1}

DRAFT

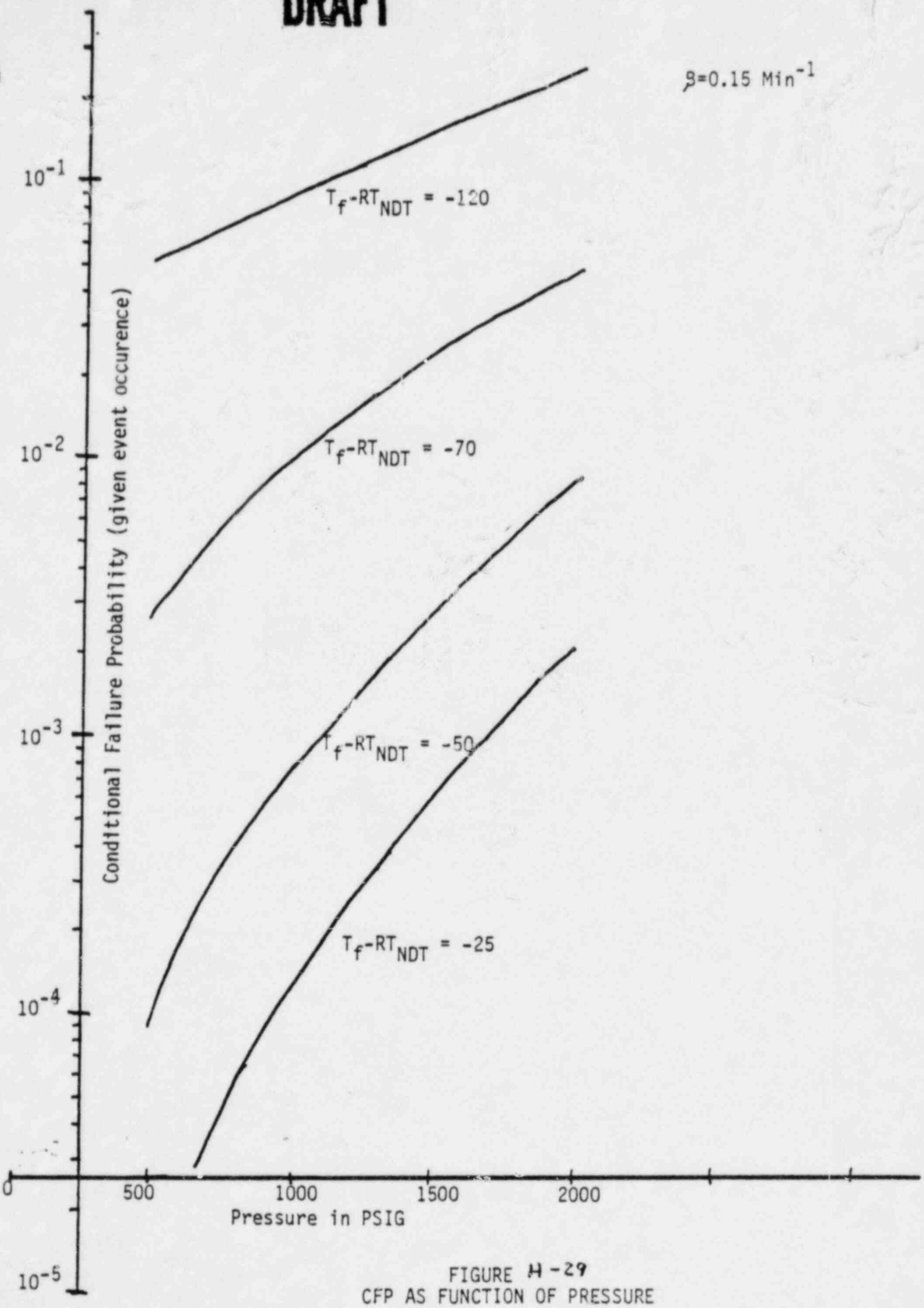


FIGURE H-29
CFP AS FUNCTION OF PRESSURE

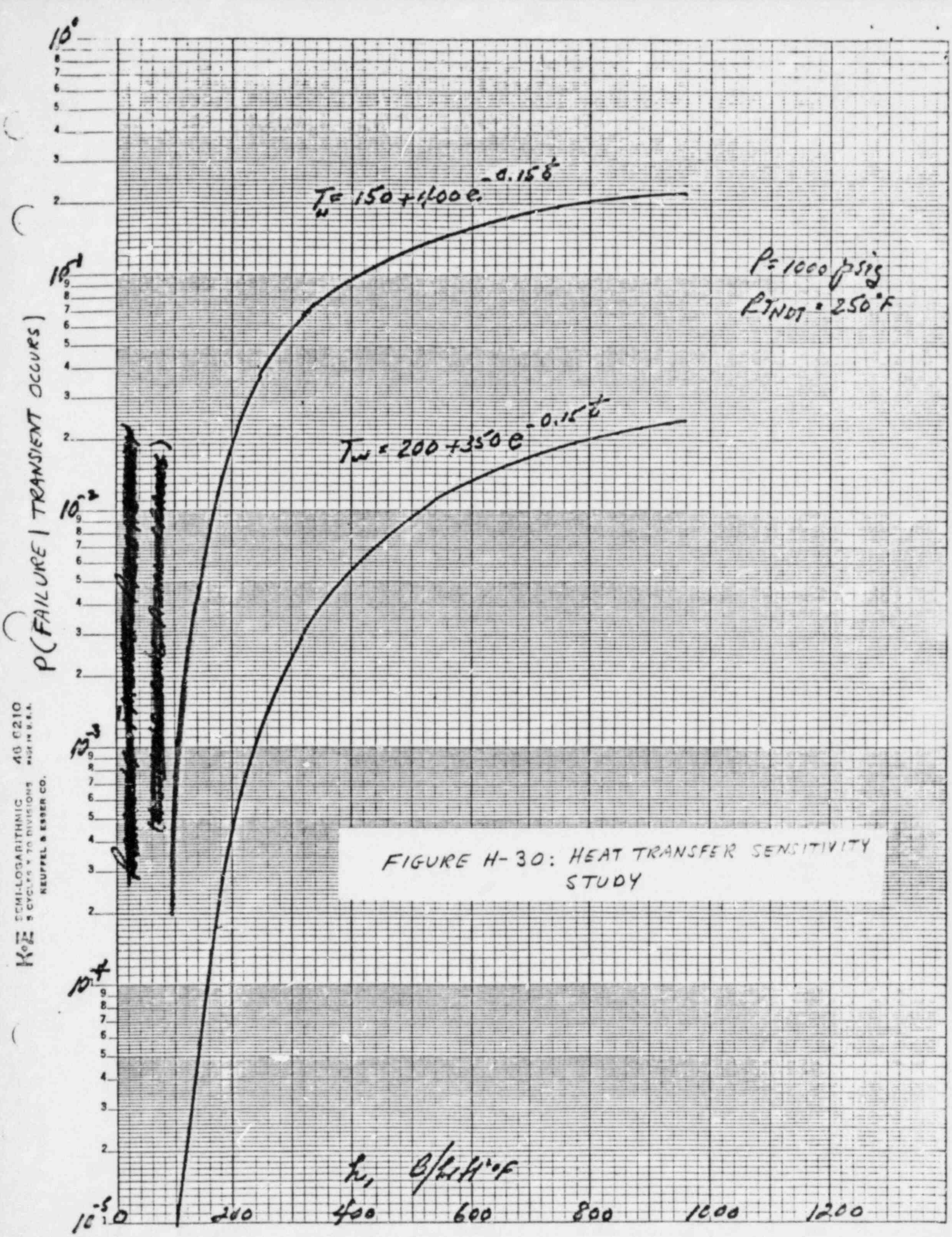


FIGURE H-30: HEAT TRANSFER SENSITIVITY STUDY

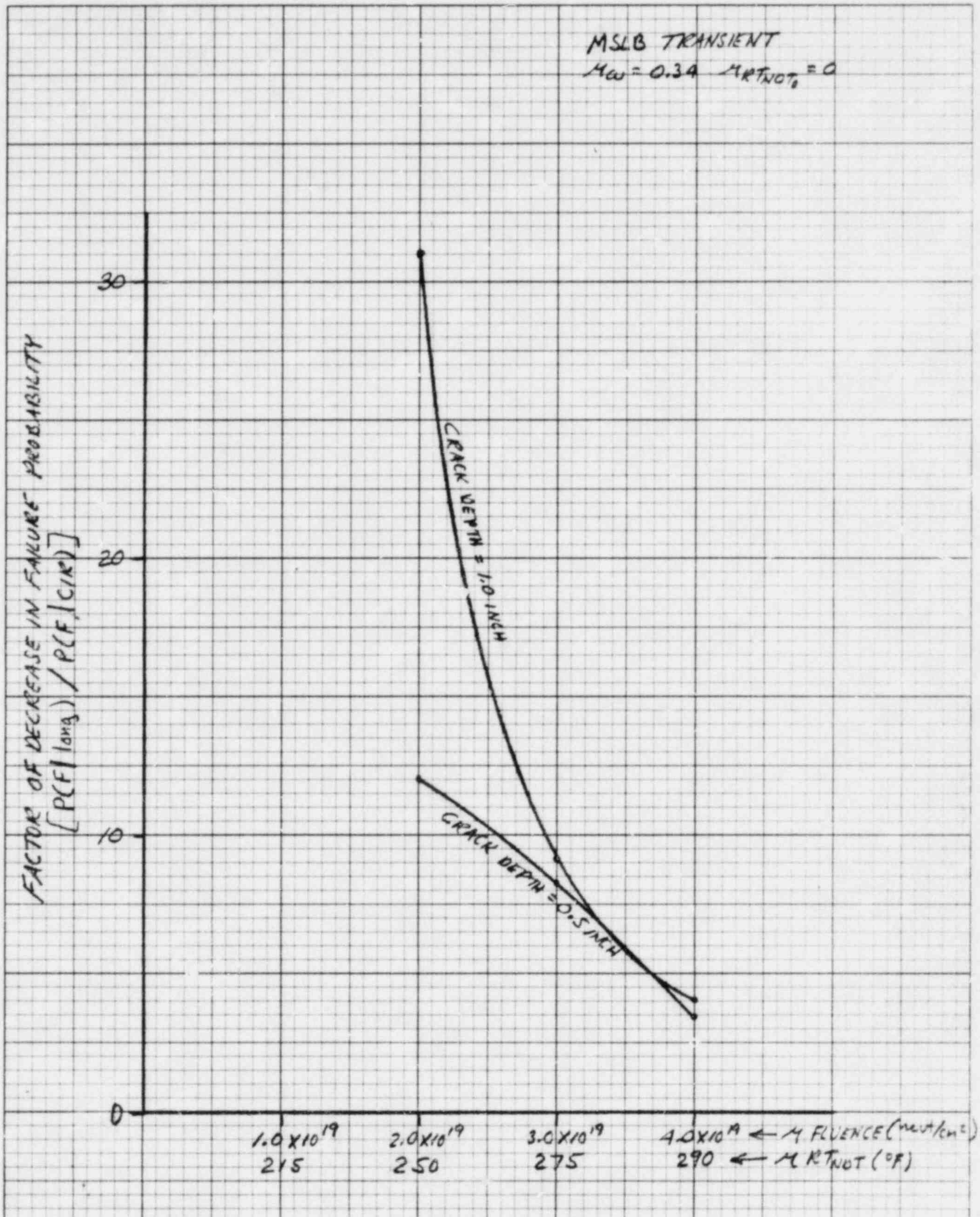


FIGURE H-31: CRACK ORIENTATION SENSITIVITY STUDY

APPENDIX I

FLUENCE RATE REDUCTION TO PWR PRESSURE VESSELS

I.1 Introduction

The NRC staff, as part of its evaluation of the Pressurized Thermal Shock (PTS) problem for PWR pressure vessels (PV), has undertaken a survey of domestic and foreign PTS experience and an evaluation of various fast neutron fluence rate reduction concepts (Ref. I.1). The survey included all three PWR vendors and the eight most affected PWR plants,* that is, those with significant vessel fluence. The staff found general agreement among those surveyed as to the techniques available for fast fluence rate reduction. The reason for this agreement is the generic similarities of the PWR plants of different manufacture and limited number of options which are considered viable.

The staff evaluation includes concepts for: (1) fluence rate reduction (by factors of 2 to 3) employing low leakage fuel loading, and (2) reductions (by factors of 10 or more) using select fuel assembly replacement on the core periphery with nonfueled assemblies containing stainless steel rods. The impact of implementing any of these schemes is so plant dependent that it was not possible to do more than estimate the impact on the total peaking factor as part of this study.

The low leakage fuel loading schemes are also characterized as an "in-out" fuel loading scheme in contrast to the usual "out-in" loading scheme. The "in-out" ("out-in") refers to fuel assembly movement during refueling from the core interior (periphery) to the core periphery (interior). In a low leakage fuel loading scheme, therefore, twice or thrice burned fuel assemblies (or even poisoned fuel assemblies) are placed on the core periphery. In our evaluation

* Fort Calhoun, San Onofre, Oconee-1, Maine Yankee, Calvert Cliffs-1, H. B. Robinson-2, Turkey Point-4 and Three Mile Island-1.

we used stainless steel rods in the nonfueled assemblies. Other choices, however, could be made for the stainless steel rods.

This report also includes a survey of foreign reactor experience with respect to fluence rate reduction to the pressure vessel.

I.2 Survey of Licensees, Owners' Groups, and Vendors

The staff visited Combustion Engineering (CE) and Westinghouse (W). Lengthy discussions were held with cognizant personnel in reactor physics, thermal-hydraulics, fuel management, and licensing. A visit could not be arranged with Babcock & Wilcox (B&W) so that information was obtained with a telephone conference call. The vendors were asked to discuss (1) the reduction of peak and longitudinal weld seam fluence accumulation rates by factors of 2, 3, 5, or 10, (2) the corresponding impact of fluence rate reduction schemes, and (3) the estimated cost of implementation of various schemes. The same questions that we asked the vendors were also asked the licensees of the eight most affected plants through the NRC project managers (Ref. I.2). These licensees had little information to offer and, generally, referred us to the respective vendors.

Limited cost estimate data was obtained from our survey. Low leakage fuel loading schemes (in-out) may result in overall cost savings to licensees because of the benefits of extended cycle operation which could accompany such schemes. However, extremes of low leakage loading schemes could cost from 1 to 5 million dollars. Replacement of fuel assemblies with stainless steel rodded assemblies on the core periphery could cost up to 20 million dollars per year due to derating plus a one-time engineering cost of 15 to 25 million dollars.

I.3 Survey of Foreign Experience

Several foreign reactor plants with radiation induced pressure vessel embrittlement have been modified or modifications are planned. Such modifications include raising the temperature of the high pressure injection water and reducing the fluence accumulation rate (i.e., lowering the fast flux to weld seams or plate material of the vessel. In the following we will deal with modifications related to fluence rate reduction to the pressure vessel. The information gathered was the result of a questionnaire directed to several countries around the world in the summer of 1981 shortly after the PTS task force was formed by NRC (Ref. I.3).

I.3.1 Finland

Loviisa-1 The Soviet built, 420 MWe Finnish reactor was put into operation in 1977 (Ref. I.4). The loading consists of about 360 hexagonal fuel assemblies. The reactor had operated for about 3 years when it was determined that the radiation induced weld seam embrittlement was higher than originally estimated. In 1980, with only 3 years of operation, the estimated nil-ductility transition temperature increase was 76°C. The originally predicted increase for 40 years of operation was 85°C.

It was decided to remove 36 fuel assemblies on the periphery of the core and replace them with hollow steel rods in a hexagonal shroud identical to that of the fuel assemblies. The assemblies that were removed represented 10% of the core inventory. However, there was no reduction in the power level because the plant had adequate thermal margin. Due to the hexagonal shape of the fuel assemblies the azimuthal flux distribution was fairly uniform varying from .73 to 1.00. The peak fast neutron flux decreased by a factor of about 7. The new flux peak appeared in the location of the previous minimum, reduced by a factor of 2.8 from .73 to about .25 (estimated nonpeak value between .22 to .30). This modification along with an increase in the temperature of the emergency injection water and changes to the emergency operating procedures is expected to provide adequate protection for the remaining life of the plant.

Loviisa-2 This is a sister plant to Loviisa-1 that was put into operation in 1980. The Finns could not decide from cost effectiveness considerations whether a modification similar to that for Loviisa-1 should have been implemented during the first cycle in Loviisa-2. Nevertheless the same modifications could be made in a later cycle.

No information is available to us on surveillance programs, neutron transport calculations, uncertainties or specific fluence values.

I.3.2 Germany (Obrigheim and Stade)

The PWR plants in the Federal Republic of Germany (FRG) have pressure vessels with only horizontal weld seams, hence, the azimuthal position of the peak fluence is immaterial* and the concern is in the irradiation of the base metal (Ref. I.8). An extensive surveillance program has been instituted in all FRG PWRs. Present estimates indicate that at the end of the 40 EFPY of operation there would be excessive irradiation of the pressure vessel of Stade and Obrigheim and that fuel assembly substitutions to lower the projected peak fluence would be needed. These reactors are very similar to Westinghouse plants, hence, we surmise that the azimuthal distribution has localized peaks. Because there is no discussion of potential consequences we assume that element substitution will be of a limited extent with no power derating. The Stade reactor has been using a low leakage loading (Ref. I.6) for the last few cycles. The estimated end of pressure vessel life fluence for Obrigheim is somewhat higher than that estimated for Fort Calhoun and for Stade is considerably lower than most American pressure vessels (Ref. I.5). The Federal Ministry of Internal Affairs of Germany in its August 10, 1981 reply to the NRC questionnaire indicated that nonfueled assembly replacement was contemplated (Ref. I.7) for these two reactors.

I.3.3 France

Recent information received from the French (Central Service for the Safety of Nuclear Installations) (Ref. I.8) indicates that a program for the study of material embrittlement was instituted about 10 years ago. This program has only recently been expanded to include pressure vessel dosimetry. No definitive plans are known at this time for pressure vessel fluence rate reduction modifications.

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*The PWR at Gundremmingen, currently under construction, has longitudinal welds.

I.3.4 Other Countries

Replies to the NRC questionnaire have been received also from Italy, Spain, Sweden, Korea, and Japan. However, none of the operating utilities have taken any steps to lower fluence rate to the pressure vessel. All show awareness of the problem. Surveillance programs have been established in Sweden and Japan.

I.4.0 Evaluation of Fast Fluence Rate Reduction Schemes

In order to assess independently a number of fluence rate reduction schemes, an evaluation was performed for the staff by its consultants at BNL (Ref. I.9). From the eight most affected PWR reactors, three plants, Oconee-1, Fort Calhoun and Robinson-2 (one from each PWR vendor), were selected for the staff evaluation. These plants were selected because of the availability of plant-specific data and the relatively large vessel fluence. Table I-1 presents some pertinent information concerning these plants. Included in the table are the vendors' and our consultant's estimate of the present and end of vessel life fluence.

The approach taken by the staff in performing this evaluation was:

(a) To use the transport theory code DOT 3.5 to calculate the fast fluence to the pressure vessel. The calculations were two-dimensional and used 16 neutron energy groups. The BNL methods have been benchmarked to a number of tests and are comparable to those used by the vendors.

(b) To use as-built dimensions, material compositions and measured values of the neutron source to evaluate H. B. Robinson-2, Oconee-1, and Fort Calhoun.

(c) To calculate for each of these plants the (1) current values of the peak fluence at the longitudinal welds, (2) projected value of the peak fluence to the end of 32 effective full power years (EFPY), (3) fluence attenuation through the pressure vessel, (4) fluence time spectra at various wall thicknesses, (5) pressure vessel fluence azimuthal distribution, and (6) end of vessel life fluence value for various fluence rate reduction schemes.

(d) To evaluate the impact of these modifications in terms of the potential increase in the total peaking factor.

(e) To compare the staff's calculations to similar calculations from the licensee or vendor when possible.

Since the fluence to the pressure vessel is caused primarily by the fast neutrons in the peripheral fuel assemblies, schemes for reducing fluence accumulation rate to the pressure vessel fall into two main classes. The first class is designed to lower the neutron leakage from the periphery of the core by lowering the power level of the peripheral fuel assemblies. The second class is designed to lower the fluence rate to the pressure vessel by placing a thick metal shield between the core periphery and the pressure vessel. This second class of fluence rate reduction schemes will not, however, be discussed further because of the lack of space between the core and vessel to accommodate large thicknesses of metal.

The first class of fluence rate reduction schemes considers the lowering of the peripheral fuel assemblies' powers by (1) using low leakage fuel loadings, and (2) removal of fuel assemblies and replacement with assemblies containing stainless steel rods. Note that the use of nonfueled assemblies contains elements of both classes of fluence rate reduction schemes. The power of the reactor could also be lowered in order to reduce the peripheral assemblies' powers. This power derating was not considered in our evaluation. Instead, the assumptions in the staff evaluation are (1) the total power of the rector is constant, (2) the shape of the power distribution remains the same from the periphery toward the center of the core, (3) the maximum linear heat generation rate is assumed constant, and (4) the core flow is assumed constant.

Since the PTS problem solution will be plant-specific, no attempt was made to optimize core fuel loading patterns on a cycle-by-cycle basis to lessen the impact on fuel cycle economics or to assess the impact on normal operation, transients, and accidents. Only a rough estimate was made of the impact in terms of a potential increase in the total peaking factor.

Some of the plant-specific factors include, among other things, the location of the weld seams in the pressure vessel, the copper and nickel content of the weld seams, the core power and size, the peripheral fuel location, the presently accumulated fluence, the fuel management scheme presently employed, and the location of the peak fluence on the vessel. An example of one of these plant-specific items is the weld seam location for the three plants. Figure I-1 shows the three weld seams at Oconee-1 folded onto a quarter core. The Fort Calhoun weld seams are shown in Figure I-2 folded onto an eighth of the core. Figure I-3 shows the weld seams at H. B. Robinson-2 folded onto an eighth of the core.

Calculations were performed by BNL for the three plants to evaluate the low leakage fuel loadings and peripheral element replacement with assemblies containing stainless steel rods. Similar results were obtained for all three plants. The conclusions of our analysis agreed wih statements made by the parties we talked to in our survey. These BNL calculations will be reported in a forthcoming BNL-NUREG report (Ref. I.13).

Table I-2 shows some results from the BNL calculations for Oconee-1 for three cases in which the ratio of the peripheral assembly power to the core average power was varied. One should roughly assume the 0.910 power ratio to be representative of normal out-in fuel assembly loading, the 0.527 ratio to represent in-out low leakage fuel loading using partially burned or poisoned assemblies, and the zero power ratio to represent peripheral fuel assemblies for which the fission source was artificially zeroed (not achievable in practice). Table I-3 shows the same results in a different format giving the fluence for the remaining 28 EFPY in terms of the relative fluence rate to the peak longitudinal weld seam for the original out-in fuel loading. Two additional cases are also shown in Table I-3. Both of these cases are representative of fuel element removal and replacement with stainless steel assemblies. In one of the cases the stainless steel rods are spaced in the same way as fuel rods while in the other case the rods are more closely packed with additional stainless steel rods. Both Tables I-2 and I-3 clearly demonstrate the fluence rate reducton factors that are possible for the two fluence rate reduction schemes. Table I-3 further demonstrates the effectiveness of including stainless steel rods in the replacement assemblies.

Figure I-4 shows in graphical form the fluence rate reduction factor for the remaining 28 EFPY (data from Table I-2) for the original peak vessel fluence location as a function of peripheral fuel assembly power. The results are linear as a consequence of our assumptions and modeling.

The fast neutron flux attenuation through the pressure vessel is shown in Figure I-5. The curve is nonlinear but shows more than a factor of 10 reduction in flux on the outside wall of the pressure vessel. This figure allows the estimation of fluence rate accumulation at various positions within the pressure vessel when the fluence rate is known on the inside wall of the vessel.

Figure I-6 provides a summary of the staff's evaluation for Oconee-1 showing results for a number of fluence rate reduction schemes as a function of effective full power years of operation. Shown in the figure are the licensee's FSAR value as well as the vendor's (B&W) estimate of the vessel fluence for the current in-out low leakage fuel loading scheme. Note that the staff's evaluation for low leakage fuel loadings closely agrees with the B&W results and both results are about half of the FSAR estimate. Three other evaluations for element removal and replacement with stainless steel assemblies are shown in the figure. Pattern 1 refers to the removal and replacement with stainless steel elements of the entire peripheral row of elements. Pattern 2 was chosen so that the fluence rate to the weld seams could be reduced with a minimum number of assembly substitutions. Pattern 3 was chosen to reduce the power peak at the core flats caused by Pattern 2. For Patterns 1, 2 and 3 fuel assembly removals and substitutions numbered 40, 20, and 32, respectively.

Figure I-7 provides a summary similar to that of Figure I-6 of the staff's evaluation for Fort Calhoun. Shown in the figure are the licensee's FSAR value as well as the vendor's (CE) estimate of the vessel fluence for the current fuel loading scheme. Note that the staff's evaluation for the current fuel loading scheme is in reasonable agreement with the CE results and both results are about a factor of 2 larger than the FSAR estimate. Staff results are also shown for two in-out low leakage schemes; in one the peripheral power is 0.41 of the core average power and in the other the peripheral power is zero. Three additional cases are shown in the figure for fuel assembly removal and replacement with stainless steel assemblies. The three cases are for the removal and replacement of 40, 24, and 16 assemblies.

Figure I-8 provides a summary similar to that of Figure I6 of the staff's evaluation for H. B. Robinson-2. Shown in the figure is the vendor's (W) estimate of the vessel fluence for the current fuel loading scheme. Note that the staff's evaluation for the current fuel loading scheme is a factor of about 1.5 larger than the vendor's evaluation. Staff results are shown for the out-in loading scheme for which the peripheral power to the core average power ratio is 0.89 as well as for two in-out loading schemes for which this ratio is 0.45 and zero. Staff results are also shown for three patterns of fuel assembly removals and replacement with stainless steel rodded assemblies. The three patterns have 36, 20, and 12 elements replaced, respectively, and represent the removal of the entire outer row of fuel as well as patterns chosen to reduce fluence rate to specific weld seams.

Table I-1 summarizes the staff survey and evaluation of the peak vessel fluence for the three plants for various schemes and the associated decrease in the total peaking factor. The table also gives present and end-of-life estimates of vessel fluence by BNL, the vendors and the FSAR value for the current fuel management scheme.

I.5.0 Conclusions

The conclusions of the staff survey and evaluation are:

(1) All vendors and licensees provided similar responses to our survey inquiries.

(2) Presently employed in-out, low leakage loading schemes provide about a 30% reduction of the fast neutron fluence rate as a side benefit derived from extended cycle core designs and may represent overall cost savings to licensees.

(3) In-out, low leakage loading schemes using twice or thrice burned fuel assemblies on the core periphery can provide a factor of 2 to 3 reduction in the fluence rate to the pressure vessel.

(4) If in addition to twice or thrice burned fuel, peripheral assemblies loaded with burnable poisons are used, a factor of 5 reduction in fluence rate can be achieved with in-out, low leakage loading schemes.

(5) If one attempted to maintain the core power rating while implementing low leakage schemes, the power distribution would become more centrally peaked and would require core redesign and fuel rearrangement to flatten power and probably would result in plant derating depending on available plant thermal margin.

(6) In-out, low leakage schemes can be used to reduce locally fluence rates in areas of peak welds, but may result in slightly higher fluence elsewhere and the appearance of peaks at new locations.

(7) The exact impact of in-out, low leakage loading schemes is plant dependent and cannot be generalized.

(8) The effectiveness of in-out, low leakage loading schemes is greatest for plants with large azimuthal flux peaks (CE & W). Implementation in B&W plants would probably involve a large number of assemblies because of the more uniform azimuthal flux distribution.

(9) Reduction of the fluence by factors of 10 or higher can be affected by peripheral assembly replacement with nonfueled assemblies (e.g., stainless steel). This can be done locally or uniformly, as needed, depending on the azimuthal flux distribution and location of weld seams.

(10) Use of nonfueled assemblies would result in significant loss of heat transfer area (10-15%), reduced core size, increased thermal peaking, increased linear power generation rates, and increased rod worths. It would require a new core design, with different fuel enrichment and new

transient and accident analyses. New limiting safety system setpoints would have to be generated and fuel management philosophies would change.

(11) Selected replacement would provide local reductions of fluence by a factor of 10 or more. If core symmetry is not maintained, however, the normal means of monitoring core power distribution based on neutron detectors and 1/8 core symmetry would have to be changed.

(12) Use of nonfueled assemblies could result in power derating of perhaps 30%.

(13) Effectiveness of any of these fluence reduction schemes depends on previous vessel exposures and materials and is less significant once significant fluence has been accumulated.

REFERENCES

I.1 Memorandum from R. J. Mattson to L. S. Rubenstein, March 13, 1982.

I.2 Letter from T. Novak to Licensees, August 21, 1981.

I.3 Letter from T. LaFleur to: Belgium, Finland, France, Germany, Italy, Japan, Korea, Netherlands, Spain, Sweden and Switzerland, with whom we have bilateral information exchange agreements, July 29 and August 20, 1981.

I.4 Institute of Radiation Protection, "Embrittlement of Loviisa-1 Reactor Pressure Vessel," Confidential Memorandum, May 15, 1981.

I.5 MPA Stuttgart, Replies to NRC Questionnaire, September 18, 1981.

I.6 Reactor Safety Commission, "Safety Against Brittle Fracture of the Reactor Pressure Vessel," NRC Translation 8-23-A, January 1981.

I.7 Letter from Schnurer of the Federal Ministry of Internal Afairs, West Germany, to LaFleur, NRC, August 10, 1981.

I.8 Letter and Attachments, to the NRC, March 19, 1982.

I.9 Letter from R. Mattson to Gregory Ogeka of DOE, "BNL Technical Assistance to the Division of Systems Integration, NRR, NRC, Pressure Vessel Radiation Embrittlement Calculations," (FIN A3387), March 19, 1982.

I.10 Letter, W. V. Parker to H. R. Denton, January 15, 1982 (Oconee Nuclear Station, Docket Nos. 50-269, 50-270, and 50-287).

I.11 Letter, W. C. Jones (Omaha Public Power District) to T. M. Novak, November 13, 1981; Letter, W. C. Jones to R. A. Clark, November 12, 1981; Letter, W. C. Jones to H. R. Denton, January 23, 1981.

I.12 H. B. Robinson Steam Electric Plant, Unit No. 2, Docket No. 50-261, License No. DPR-23, Letter, E. G. Utley to D. G. Eisenhut entitled, "Thermal Shock to Reactor Pressure Vessels," January 25, 1982.

I.13 Report on the PTS Audit Calculations by BNL in a BNL-NUREG report to be published.

Table I.1 Vessel fluence for Oconee-1, Ft. Calhoun and H. B. Robinson-2

	Oconee-1	Ft. Calhoun	Robinson-2
Total Effective Full Power Years of Operation (EFPY) As of 12/81	5.1	5	7
(a) Out-in loading EFPY	4	5	7
(b) In-out low leakage EFPY	1.1	-	-
Present Vessel Fluence Using Current Fuel Loading Scheme ($\times 10^{18}$ n/cm^2)			
(a) BNL calculation	2.70	7.24	21.3
(b) Vendor calculation	2.55 (Ref. I-10)	6.60 (Ref. I-11)	13.8 (Ref. I-12
End of Vessel Life Fluence ($\times 10^{18}$ n/cm^2)			
I. Using Current Fuel Loading Scheme			
(a) BNL calculation	12.1*	45.9	97.1
(b) Vendor calculation	12.5 (Ref. I-10)	42.0 (Ref. I-11)	65.6 (Ref. I-12
(c) FSAR value	22.0	20.0	51.0
*II. Using In-out low leakage loading scheme/	11.2	30.0	53.7
(Increase in Total Peaking Factor (%))	(7)	(17)	(17)
**III. Using Stainless Steel Assemblies On Periphery/	1.90	12.7	28.1
(Increase in Total Peaking Factor(%))	(23)	(30)	(23)

*Out-in loading scheme value is 18.5×10^{18} n/cm^2.

**BNL calculation.

Table I-2 Staff evaluation of flux and fluence to weld seams and peak fluence location for Oconee-1

	Weld* SA-1430	Weld SA-1493	Weld SA-1073	Peak Wall Location
I. Flux ($\times 10^{10}$ n/cm^2-sec)				
Fuel Loading method				
(a) Out-in, $P/\overline{P}$ = 0.910	1.59	1.37	1.45	1.84
(b) Low leakage, $P/\overline{P}$ = 0.527	.984	.915	.911	1.11
(c) Low leakage, $P/\overline{P}$ = 0.0	.124	.188	.125	.188
II. Fluence for 28 EFPY ($\times 10^{18}$ n/cm^2)				
Fuel loading method				
(a) Out-in, $P/\overline{P}$ = 0.910	13.5	11.7	12.3	15.6
(b) Low leakage, $P/\overline{P}$ = 0.527	8.35	7.76	7.73	9.41
(c) Low leakage, $P/\overline{P}$ = 0.0	1.05	1.60	1.06	1.60
III. Fluence for 32 EFPY ($\times 10^{18}$ n/cm^2)				
Fuel Loading Method				
(a) Out-in, $P/\overline{P}$ = 0.910	16.1	13.9	14.6	18.5
(b) Low leakage, $P/\overline{P}$ = 0.527	9.93	9.23	9.20	11.2
(c) Low leakage, $P/\overline{P}$ = 0.0	1.25	1.90	1.26	1.90

*See Figure I-1 for weld seam location.

Table I-3 Staff evaluation of the ratio of the fluence rate to the weld seams and the original peak fluence location to the fluence rate of Weld Seam SA-1430 for the remaining 28 EFPY for Oconee-1

Fuel Loading Method	Relative Fluence and Fluence Rate Reduction Factor*			
	Weld SA-1430	Weld SA-T493	Weld SA-1073	Original Peak Fluence Location
Out-in, $P/\bar{P}$ = 0.91	1.00 / 1.00	.863 / 1.16	.909 / 1.10	1.15 / .87
In-out, low leakage $P/\bar{P}$ = 0.527	.618 / 1.62	.575 / 1.74	.572 / 1.95	.697 / 1.43
In-out, low leakage $P/\bar{P}$ = 0.0	.078 / 12.8	.118 / 8.47	.078 / 12.8	.098 / 10.2
Stainless Steel Assemblies, $P/\bar{P}$ = 0.0	.049 / 20.4	.103 / 9.71	.057 / 17.5	.077 / 13.0
Stainless Steel Assemblies,** $P/\bar{P}$ = 0.0	.033 / 30.3	.081 / 12.3	.040 / 25.0	.061 / 16.4

*First number is the fluence rate ratio; the second number is the fluence rate reduction factor.

**This case includes nonfueled assemblies with additional stainless steel rods in a close packed array.

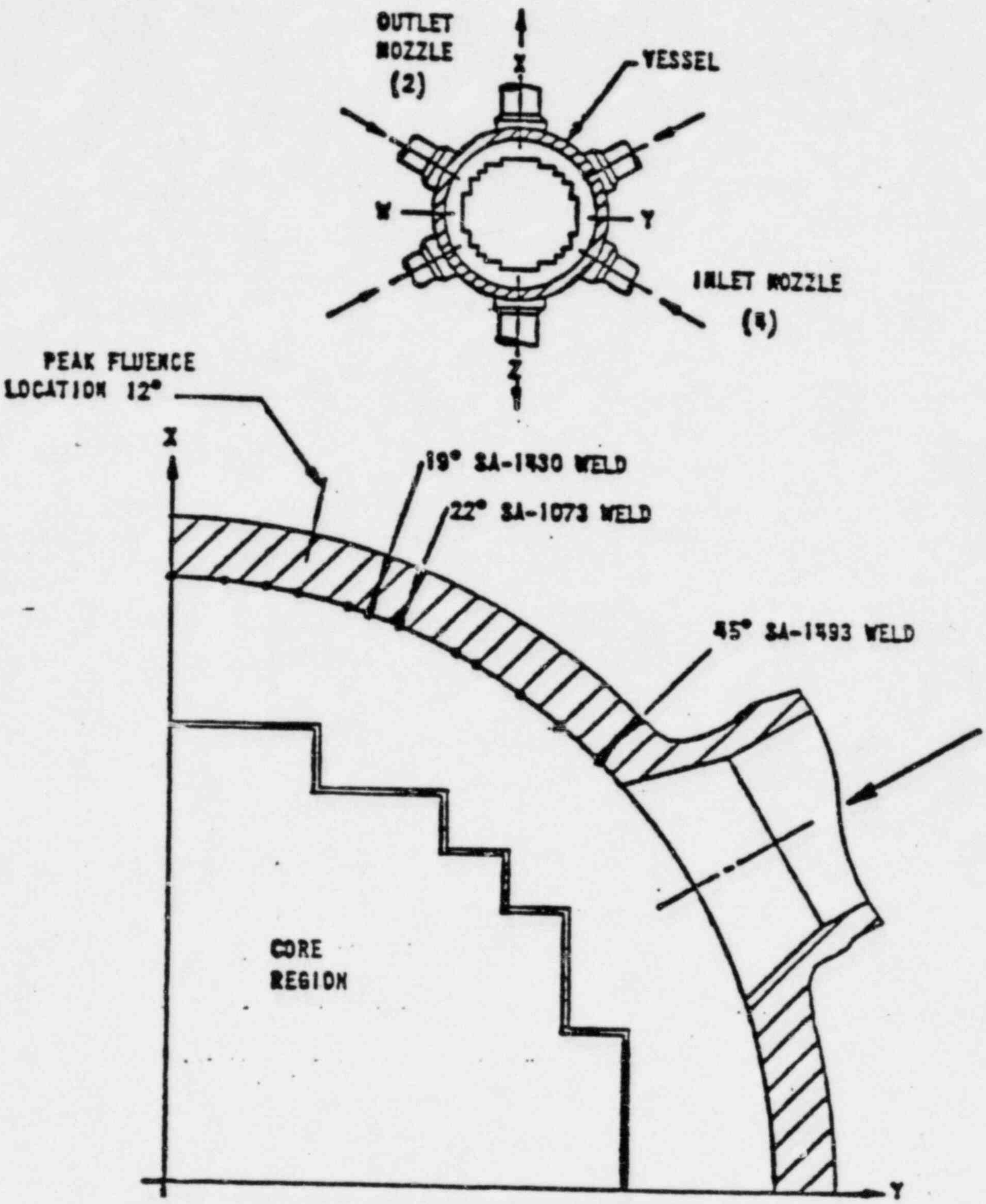


FIGURE-1 LONGITUDINAL WELD LOCATIONS FOR OCONEE-1

I-1

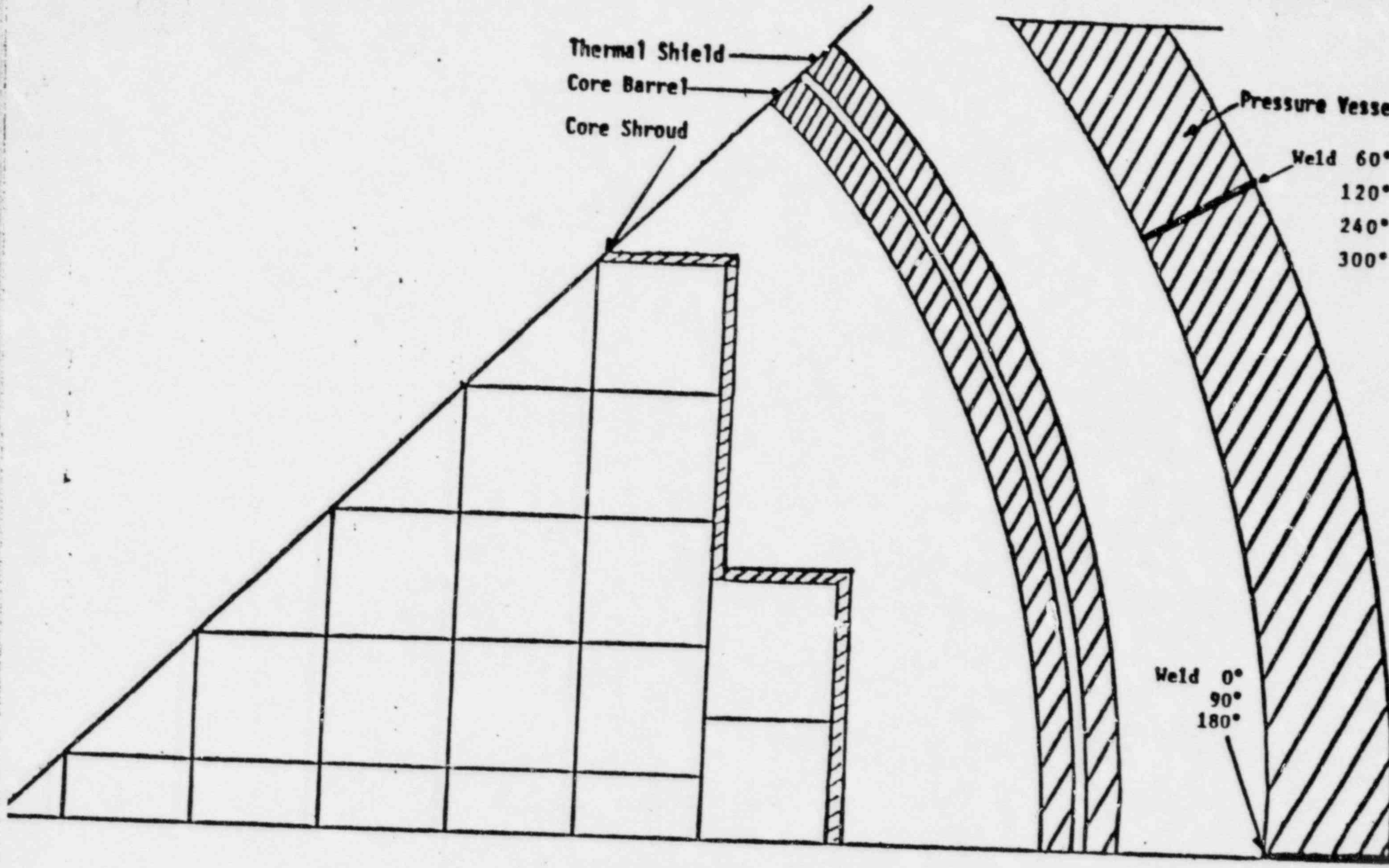


FIGURE-2 FUEL ELEMENT GRID AND LONGITUDINAL WELD SEAMS FOR FORT CALHOUN

I-2

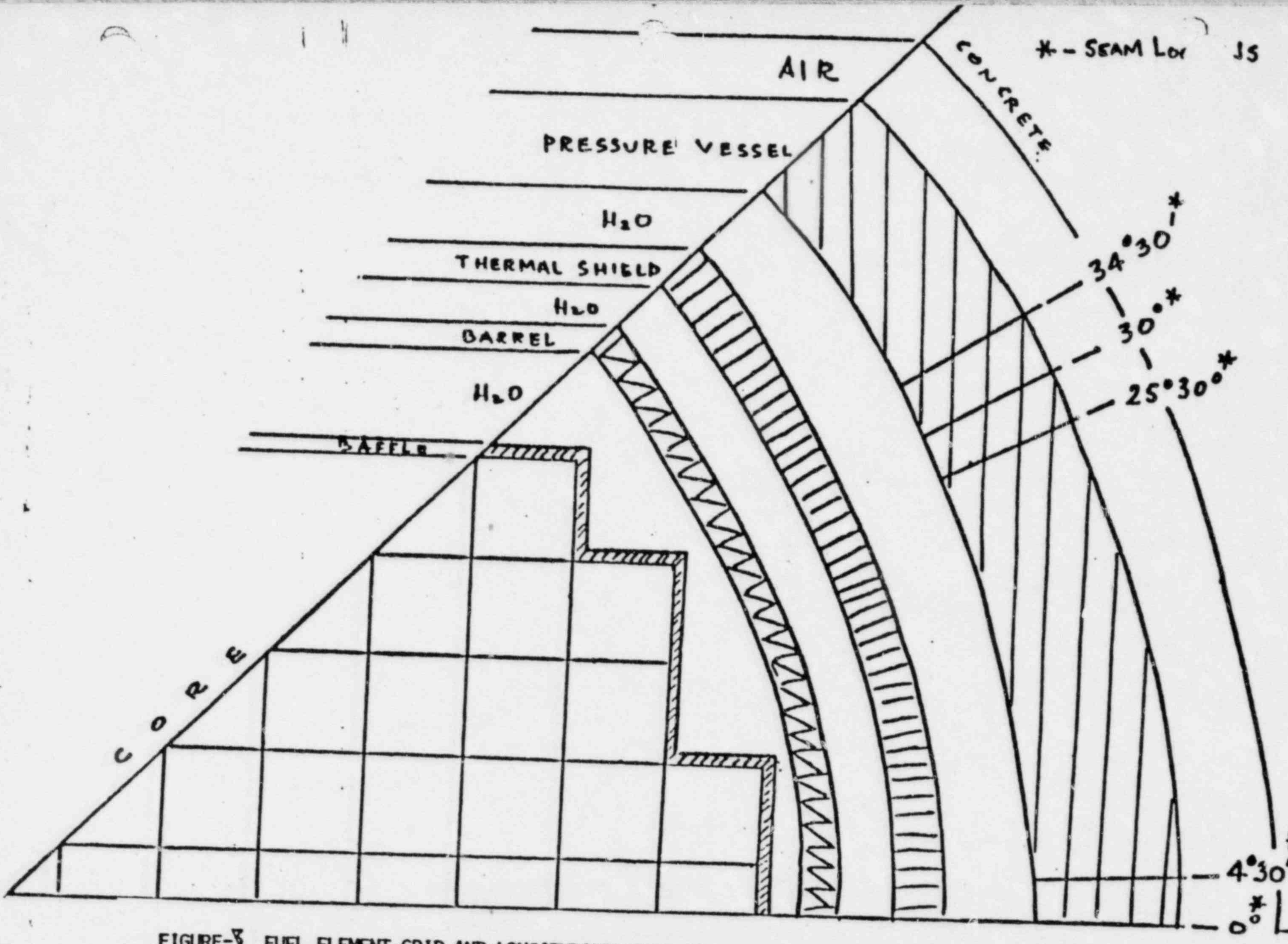


FIGURE-3 FUEL ELEMENT GRID AND LONGITUDINAL WELD SEAM LOCATIONS FOR H. B. ROBINSON-2

I-3

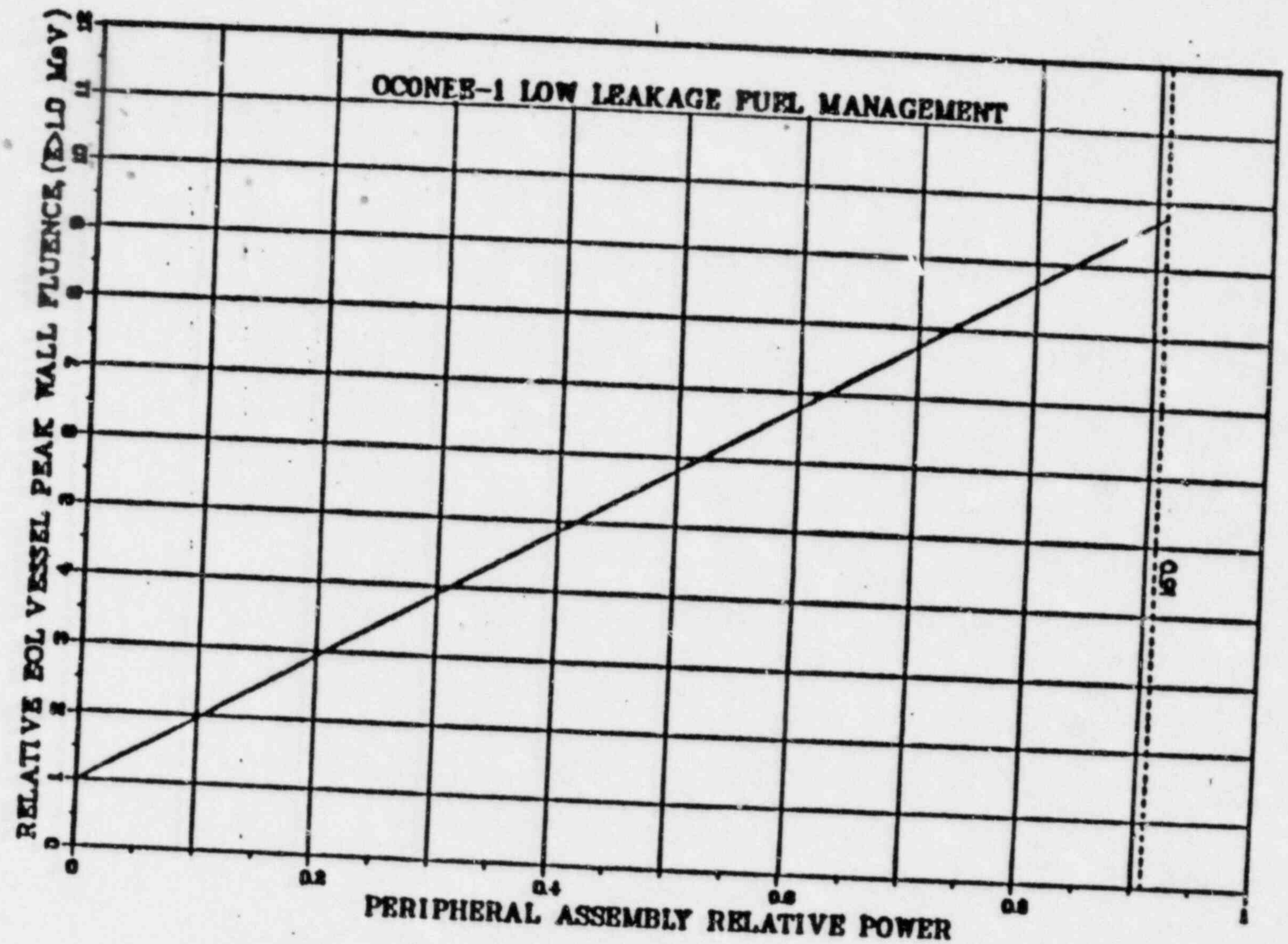


FIGURE-4 PEAK WALL FLUENCE AS A FUNCTION OF PERIPHERAL ASSEMBLY POWER FOR OCONEE-1 (FOR REMAINING 28 EFPYS)

I-4

I-20

469

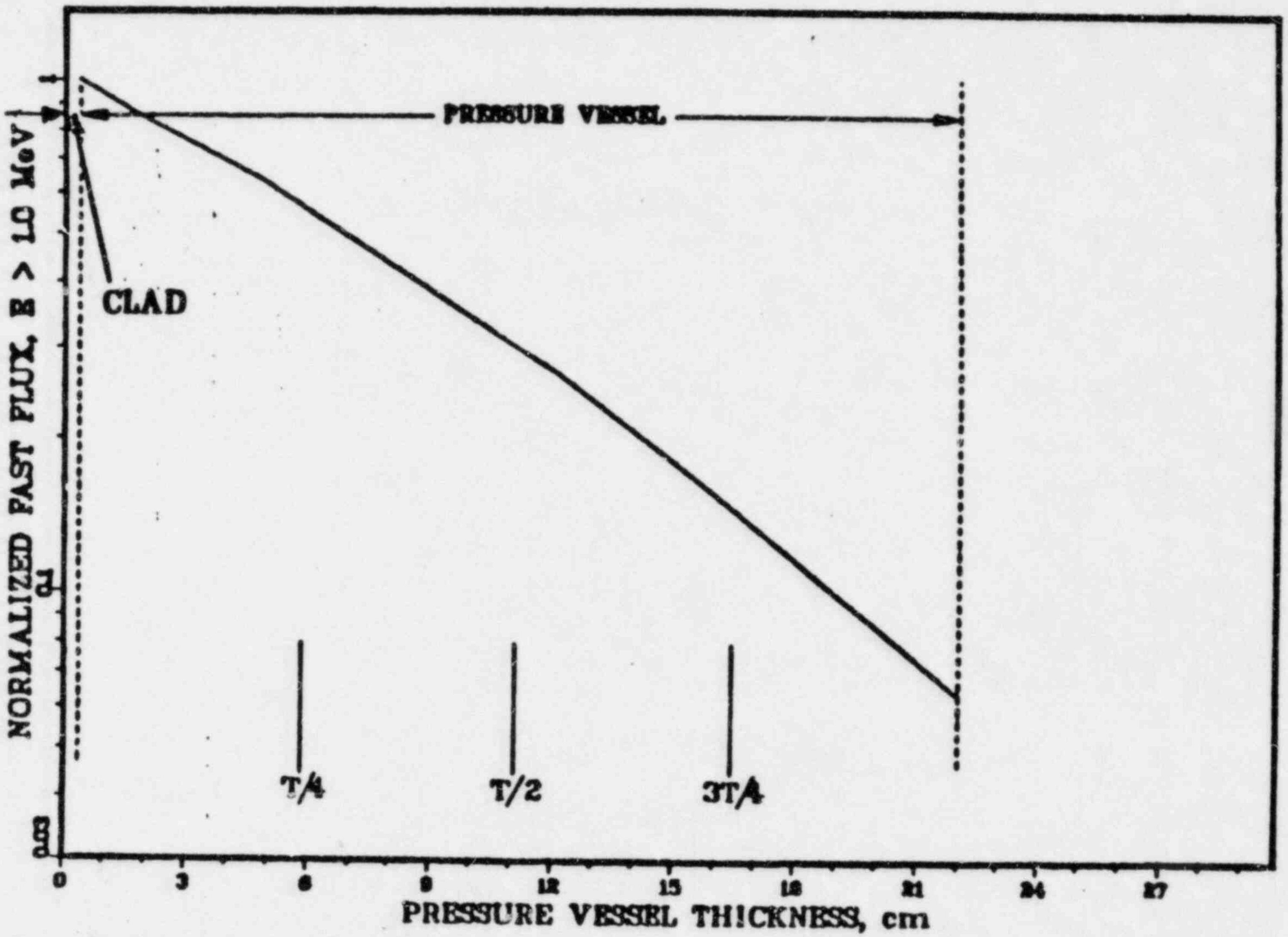


FIGURE-5 FAST FLUX ATTENUATION THROUGH THE PRESSURE VESSEL WALL FOR OCONEE-1

I.5

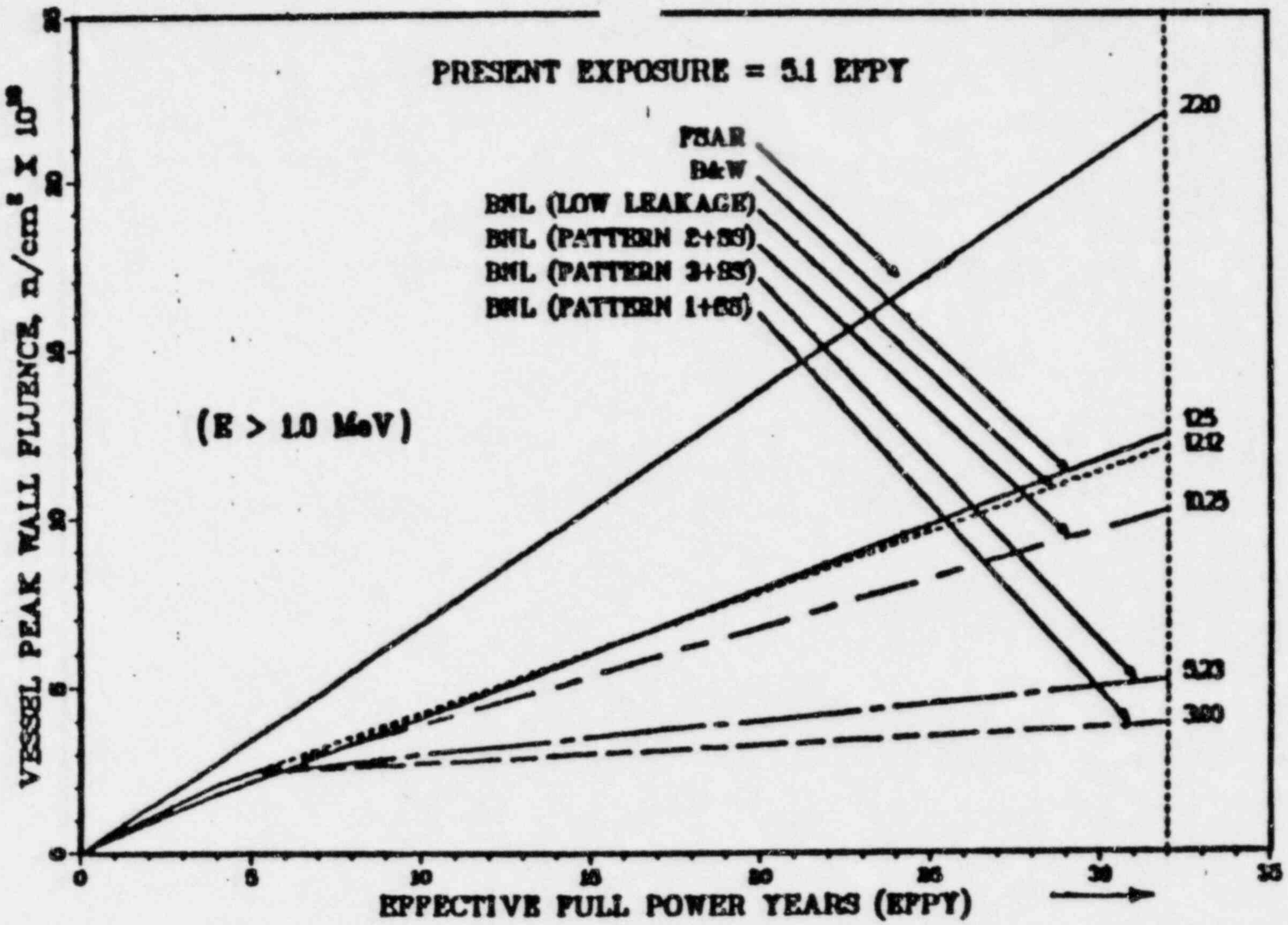
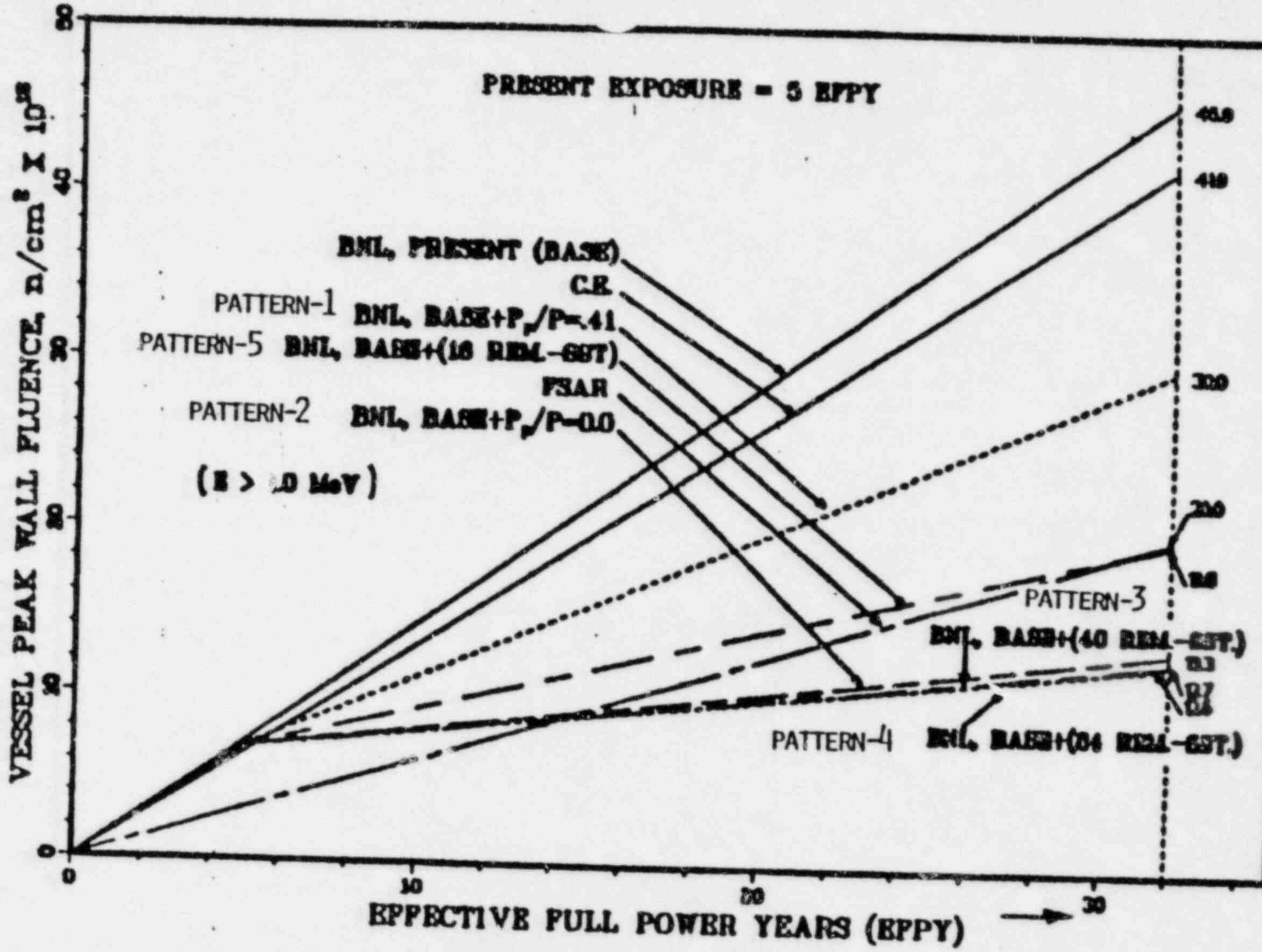


Figure-I-6 Peak Fluence As A Function of EFPY For Various Fluence Rate Reduction Schemes For Oconee-1

Cop

Pattern 1 Removal and replacement of outer row of assemblies wibh 40 stainless steel assemblies
Pattern 2 Removal and replacement of 20 assemblies opposite weld seams
Pattern 3 Modification of Pattern 2 with additional removals and replacement of peripheral assemblies (32 total)

4/31/22



Caps Figure 7 Peak Fluence As A Function of EFPY For Various Fluence Rate Reduction Schemes For Ft Calhoun

I-7

Pattern 1 Low leakage scheme(peripheral to core average power = 0.41)
Pattern 2 Low leakage scheme(zero peripheral power)
Pattern 3 Assembly replacement with 40 stainless steel assemblies
Pattern 4 Assembly replacement with 24 stainless steel assemblies
Pattern 5 Assembly replacement with 16 stainless steel assemblies

I-23

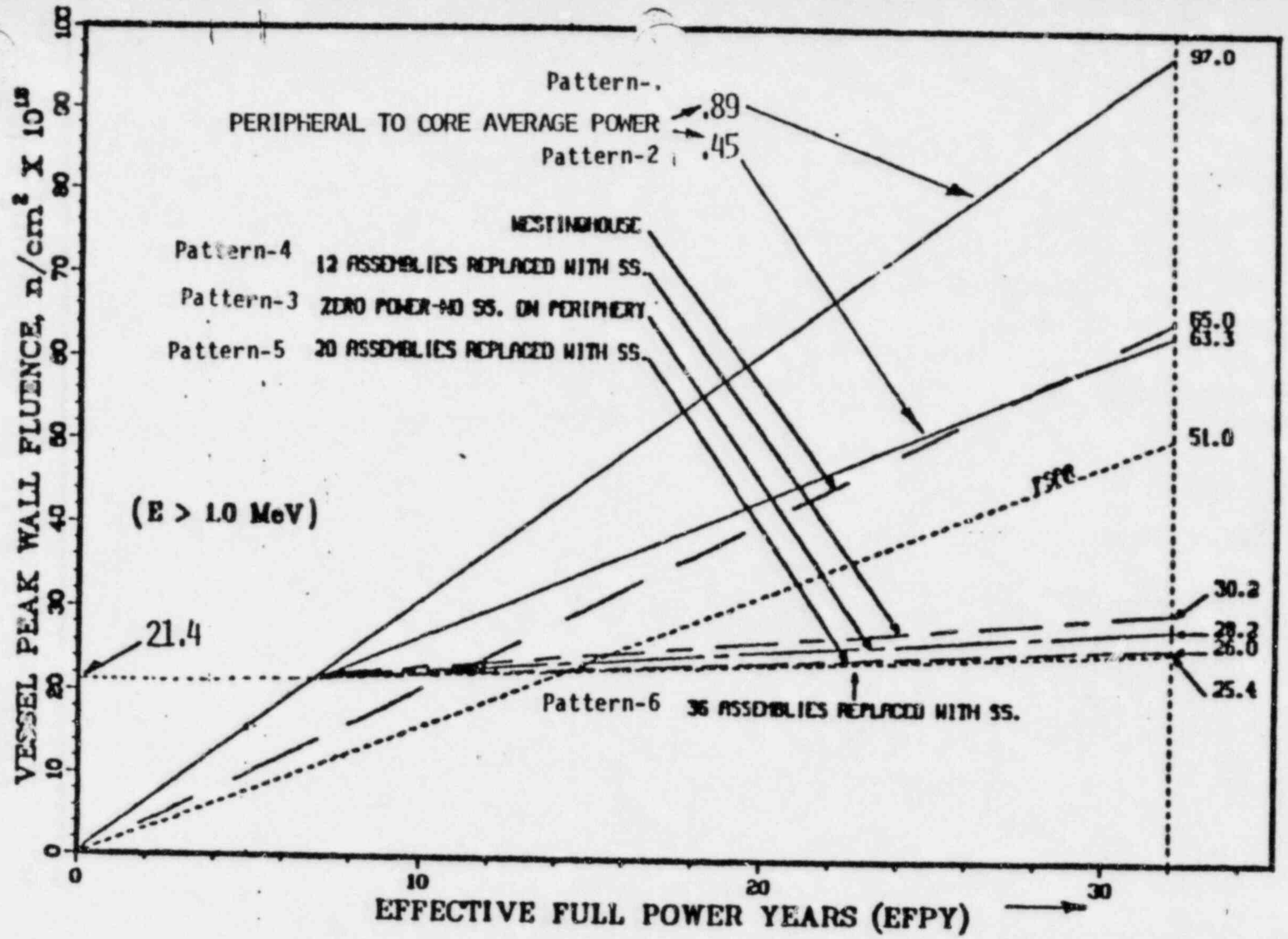


Figure-8 Peak Fluence As A Function of EFPY for Various Fluence Rate Reduction Schemes for Robinson-2

Cap I-8

Pattern-1 Out-in fuel loading, $P/\overline{P}$ = 0.89
Pattern-2 Low leakage loading, $P/\overline{P}$ = 0.45
Pattern-3 Low leakage loading, $P/\overline{P}$ = 0.0
Pattern-4 Fuel assembly replacement with 12 stainless steel assemblies
Pattern-5 Fuel assembly replacement with 20 stainless steel assemblies
Pattern-6 Fuel assembly replacement with 36 stainless steel assemblies

I-29

APPENDIX J

SUMMARY OF NRC STAFF POSITION ON REVIEW OF CONTROL SYSTEMS

J.1 Introduction

The following summarizes the staff philosophy on the review and control and protection systems and delineates actions completed or planned to address the effects of control systems on plant safety. The following also specifically discusses the possible impact of control system failures on pressurized thermal shock and actions which should be considered to minimize the possibility of control system failures resulting in an excessive plant cooldown transient.

J.2 Philosophy of Separation of Protection and Control Systems

The philosophy on the separation of protection systems and control systems was developed in the 1960's and early 1970's through interactions between the regulatory staff and industry. The interactions occurred primarily through the development of industry standards such as IEEE-279 "Criteria for Protection Systems for Nuclear Power Generating Stations." The staff did not dictate a particular philosophy, but rather explored through the standards committees and early plant licensing reviews various approaches which could be taken toward reactor protection.

A brief, simplified description of the approach toward protection and control is as follows. A nuclear power plant must satisfy utility requirements for the economic production of power. These requirements include plant operation with a limited number of operators, high plant availability with few unplanned shutdowns, and the ability to follow the utility grid load demand. The requirements for operation are based largely on matching the capabilities of nonnuclear plants. Plant control systems to accomplish the desired economic operational characteristics are established. The control systems, of course, have to be capable of allowing the plant to perform normal operations with margin to plant safety limits.

To assure that safety limits are not exceeded should any system used for normal operation fail, various protective functions such as reactor trip and decay heat removal have been established in the Commission regulations. Systems whose primary purpose is to accomplish the protective functions are provided to fulfill these requirements.

One, thus, has two somewhat differing objectives. The first is to allow normal plant operation within a utility grid which is also supplied by many non-nuclear plants. For this, control functions have been established. The second objective is to ensure that even with failures of the operational equipment, safety limits are not exceeded. For this, protective functions have been established to assure plant safety.

Once control functions and protective functions are defined, a decision has to be made as to whether the same systems should be used for both or whether separate systems should be used. The philosophy developed through the standards committees was one in which the protection systems were treated separately. This allowed a set of guidelines to be established with the intent of ensuring that protection functions are accomplished with a very high degree of reliability. Having a specific, well-defined group of protection systems to accomplish required safety functions allows both industry and the regulatory agency to concentrate their efforts and make effective use of limited resources in accomplishing safety goals.

In development of the philosophy, it was recognized that some limited ties between protection systems and control systems are appropriate and even unavoidable. For example, the systems will always be interrelated through the fluid process systems. Additional interfaces such as the use of the same sensors for protection and control were considered acceptable providing appropriate rules are followed. General Design Criterion 24 and IEEE-279 permit limited interconnections between protection and control systems and define rules for implementing these interconnections.

J.3 NRC Staff Reviews of Control Systems

NRC staff reviews have been performed on currently licensed plants with the goal of ensuring that control system failures will not prevent automatic or manual initiation and operation of any safety system equipment required to trip the plant or maintain the plant in a safe shutdown condition following any "anticipated operational occurence" or "accident." The approach has been to either provide independence between safety and nonsafety systems or to require isolating devices such as isolation amplifiers between safety and nonsafety systems such that failures of nonsafety system equipment cannot propagate through the isolating devices to impair operation of the safety system equipment. In addition, a specific set of "anticipated operational occurrences" and "accidents" have been analyzed to demonstrate that plant trip and/or safety system equipment actuation occurs with sufficient capability and on a time scale such that the consequences are within specified acceptable limits. In these analyses, conservative initial plant conditions, core physics parameters, equipment availability, and instrumentation setpoints have been assumed. Conservative parameters (for example, heat fluxes, temperatures, pressures, and flows) which could result in core or reactor coolant system pressure boundary damage are also assumed. Where active control system operation would mitigate the consequences of a transient, in general, no credit is taken for the control system operation. In some cases, credit has been allowed for the operation of specific control systems in mitigating the consequences of particular "anticipated operational occurrences." Where this has been allowed, special design features and/or technical specification requirements such as periodic testing have been provided.

Where active control system operation would not mitigate the consequences of a transient, no penalties are taken in the analyses for incorrect control system actions caused by control system failures. In the case of control systems, for example, the loss of forced reactor flow is analyzed assuming the reactivity control systems either operate properly or do not operate at all, whichever is the worst case. A loss of forced reactor flow occurring simultaneously with an inadvertent rod withdrawal is not considered. Among the specified set of "anticipated operational occurrences" analyzed are occurrences resulting from both mechanistic and nonmechanistic control system failures. The conservative analyses performed are intended to demonstrate that the potential consequences to the health and safety of the public are within acceptable limits for a wide range of postulated events even though specific actual events might not follow the same assumptions made in the analyses.

In general, until approximately one year ago systematic evaluation of control systems designs had not been performed to determine whether single event induced multiple control system actions could result in a transient such that core or reactor coolant system pressure boundary limits established for "anticipated operational occurrences" are exceeded. Single failures or events which could induce multiple control system actions such as discussed above do indeed exist, experience with operating plants indicates that incidents resulting in transients more severe than currently analyzed as "anticipated operational occurrences" have a low probability. Recent operating plant license applicants have been required to address the possibility of multiple control system actions caused by certain specified events such as a power supply failure or sensor impulse line failure.

The applicants have been required to identify any power sources, sensors, or sensor impulse lines which provide power or signals to two or more control systems and demonstrate that failures of these power sources, sensors, or sensor impulse lines will not result in consequences more severe than those bounded by the analyses of "anticipated operational occurrences" in Chapter 15 of the FSAR. At this time, similar reviews have not been required of operating plant licensees. However, the effort on the current license applications will provide general guidance on whether significant problems may exist on operating plants.

Until approximately two and one-half years ago systematic evaluations of control system designs had not been performed to determine whether postulated accidents could cause control system failures resulting in control actions which would make accident consequences more severe than presently analyzed. Accidents could cause control system failures by creating a harsh environment in the area of the control equipment or by physically damaging the control equipment. Licensees have, however, now reviewed the possibility of consequential control system failures which exacerbate the effects of high energy line breaks and taken action, where needed, to assure that the postulated events would be adequately mitigated. Similar efforts are also being performed on plants currently under operating license review.

It should be emphasized that the issue is not whether reactor trip or safety system equipment action would be defeated by control system failures, but whether control system failures could cause a transient or accident to proceed in a manner potentially more severe than currently analyzed. Systematic reviews of safety systems have been performed with the goal of ensuring that control system failures (single or multiple) will not defeat trip or safety system action, and both industry standards and staff regulatory guides are quite clear that this is a design requirement for safety systems including those used for reactor trip.

J.4 Instrumentation and Control System Impact on Pressurized Thermal Shock

Control system failures can cause inadvertent reactor coolant system cooldowns and inadvertent increases in reactor coolant system pressure. Whether any credible control system failures can cause unacceptable reactor coolant system temperature/pressure combinations, however, requires further analyses.

There are control system failures which can cause excessive feedwater flow or abnormally low feedwater temperature, either of which could lead to reactor coolant system cooldown. If it is found necessary through review of limiting transients, feedwater flow can be terminated automatically with safety-grade equipment following detection of an excessive cooldown. If the problems of concern are found to be only with the control system (and not, for example, with feedwater valve failures) then safety-grade interlocks could be used to redundantly override the control system and terminate feedwater. If there is a concern with excessive feedwater caused by valve malfunction (such as a feedwater control valve failing open) feedwater could be terminated with safety-grade equipment by closing redundant valves or by tripping feedwater pumps and closing a single set of valves for redundancy. This method of terminating feedwater flow could, however, require the addition of expensive equipment on some plants. Also, analyses would have to be performed to determine if feedwater pump trip or valve closure could be accomplished sufficiently rapidly to mitigate any transient of concern.

There are control system failures which can cause excessive steam flow through electric, air, or hydraulic operated steam valves which could lead to reactor coolant system cooldown. As with the feedwater flow, steam flow could be terminated with safety-grade interlocks or safety-grade isolation valves following detection of excessive cooldown. If a cooldown transient, however, is initiated by a "stuck open" safety valve, it could not be terminated by safety system equipment since design codes prohibit isolation valves in series with safety valves.

Inadvertent reactor coolant system pressure increases caused by control system failure can be terminated by redundantly turning off pressurizer heaters or redundantly terminating charging flow if shown to be necessary. However, it should be noted that inadvertent cooldowns of sufficient magnitude will, in general, result in eventual automatic initiation of safety injection which, in turn, results in an increase in reactor coolant pressure if operator action is not taken.

A number of plants currently employ interlocks and valves which are redundant and at least "quasi-safety-grade" to automatically terminate feedwater flow and/or steam flow under conditions which could lead to inadvertent cooldown, overfill of steam generators (PWRs), or overfill of reactor vessels (BWRs).

In addition to inadvertent cooldowns or increases in pressure which can be caused by control system failures, actuation of certain emergency safeguards systems can cause inadvertent cooldown and consequential increase in reactor coolant pressure. For example, actuation of auxiliary feedwater on a PWR following a reactor trip can cause an inadvertent reactor coolant system cooldown, contraction of water in the reactor coolant system, depressurization of the reactor coolant system, automatic actuation of safety injection, and then a repressurization of the reactor coolant system. This could occur if operator action is not taken to manually control auxiliary feedwater after its automatic initiation. During recent operating license reviews, the Instrumentation and Control Systems Branch has been reviewing the circuits, equipment, and indications used by the operator to control auxiliary feedwater after automatic initiation with the goal of ensuring that a single failure will not cause uncontrolled auxiliary feedwater flow. A staff position on the design of the auxiliary feedwater system, including instrumentation and controls, has been proposed and is currently under review by the Division of Safety Technology. Implementation of this position would significantly improve the failure tolerance of the auxiliary feedwater system from the standpoint of failures which could result in excessive plant cooldown.

J.5 Actions Completed or Underway to Detemine Potential Consequences of Control System Failures

The consensus judgment of the NRC staff continues to be that the risk associated with control system failures is not sufficient to require immediate corrective actions. However, to provide added assurance, the following actions are being or have been taken:

(1) The resolution of Unresolved Safety Issue A-47, "Safety Implications of Control Systems" will systematically determine if current licensing practices with repect to control systems are adequate. The plan for resolution of this issue specifically addresses evaluations to determine any actions required to prevent control system failures from causing unacceptable reactor coolant system cooldown or overfill of a steam generator (PWR) or reactor vessel (BWR).

(2) Standard Review Plan Section 7.7 calls for staff reviews to assure that failures of control systems will not impair the capability of the protective system in any significant manner or cause plant conditions more severe than those for which the plant safety systems are designed. The staff has pursued these reviews primarily to ensure that electrical interconnections between protection systems and control systems are implemented such that failures in control system equipment cannot impair the operation of protection system equipment. The Chapter 15 design-basis event analyses have also been reviewed to assure that sufficient conservatism has been assumed so that these analyses adequately bound the consequences of single control system failures. The Instrumentation and

Control Branch has been reviewing control system designs of operating license applicants to confirm that the Chapter 15 design bases analyses also bound multiple control system failures initiated by credible failures of common power sources, sensors, or sensor impulse lines. In addition, operating license applicants have been requested to review the potential for control system malfunctions caused by high energy line breaks. Section 7.7 of the Standard Review Plan was revised in 1981 to be more explicit on criteria applicable to control systems. Specifically, the criteria shown in the attached table are now delineated in Section 7.7 and reviews of plants currently under licensing review are performed with the goal of verifying that the criteria are met.

(3) In September 1979, all licensees were asked to review the possibility of consequential control system failures which could exacerbate the effects of high energy line breaks and identify appropriate actions, where needed, to assure that these events would be adequately mitigated. The review was requested as a result of postulated scenarios involving consequential control system failures identified by Westinghouse. All licensees responded to the request and the responses were screened. On the basis of the review, no specific event leading to unacceptable consequences was identified and, in general, control equipment locations were such that consequential failures would be unlikely. Some licensees, however, did make changes to operating procedures to address the possibility of control failures.

(4) I&E Bulletin 79-27 was issued to licensees requesting that evaluations be performed to ensure the adequacy of plant procedures for accomplishing shutdown upon loss of power to any electrical bus supplying power for instruments and controls. In their response to the bulletin, licensees have indicated that corrective action has been taken including hardware changes and revised procedures, where required to assure that the loss of any single instrument bus would not result in the loss of instrumentation required to mitigate such an event. As part of operating license reviews, we are requesting similar verification by operating license applicants.

(5) Implementation of Regulatory Guide 1.97, "Instrumentation for Light-Water-Cooled Nuclear Power Plants to Assess Plant and Environs Conditions During and Following An Accident," and NUREG-0737, "Clarification of TMI Action Plan Requirements," will significantly upgrade both the quantity and quality of information available to the operator to diagnose and respond to control system failures.

(6) In 1979 B&W completed a failure modes and effects analysis and review of operating experience for their Integrated Control System (ICS) and reported the results in B&W Report BAW-1564, "Integrated Control System Reliability Analysis." B&W made several recommendations regarding control system improvements which could be made to improve overall plant performance. Licensees with B&W plants were requested to evaluate the B&W recommendations and report their follow-up actions to the staff. Responses were received and reviewed. Based on the review of BAW-1564 and the responses to the B&W recommendations, the staff has not identified any specific control system failures or actions that would lead to unacceptable consequences.

(7) The Office of Standards Development is coordinating efforts with the IEEE to establish design criteria for systems important to safety which are not covered by and do not need to meet all of the rigorous standards for safety system equipment but nevertheless may be sufficiently important to safety to be included in the NRC review process.

J.6 Conclusions

At this time, the staff knows of no specific control system failures or actions which would lead to unacceptable consequences. A variety of efforts are still underway to determine the potential safety consequences of control system failures including their impact on pressurized thermal shock. Should these reviews indicate that additional criteria for control system designs are necessary or that specific problems require resolution, appropriate action will be taken for plants in the licensing process and for plants now in operation.

J.6.1 Standard Review Plan Guidance for Control System Review

(1) Confirm That The Plant Accident Analyses in Chapter 15 of the SAR Do Not Rely On The Operability Of Control Systems To Assure Safety.

(2) Confirm That The Safety Analyses Include Consideration Of The Effects Of Both Control Systems Action And Inaction In Assessing The Transient Response Of The Plan For Accidents And Anticipated Operational Occurrences.

(3) Confirm That Consequential Effects Of Anticipated Operational Occurrences And Accidents Do Not Lead To Control Systems Failures Which Would Result In Consequences More Severe Than Those Bounded By The Analyses In Chapter 15 Of The SAR.

(4) Confirm That The Failure Of Any Control System Component Or Any Auxiliary Supporting System For Control Systems Will Not Cause Plant Conditions More Severe Than Those Bounded By The Analyses Of Anticipated Operational Occurrences In Chapter 15 Of The SAR (The Evaluation Of Multiple Independent Failures Is Not Intended).

APPENDIX K

EFFECTS OF HEATING ECCS WATER ON PRESSURIZED THERMAL SHOCK

Increasing the temperature of the ECCS water can have a positive effect on PTS for LOCA events, where the dominant overcooling results from the injection of the cold ECCS water.

K.1 Large- and Small-Break LOCAs and Secondary Side Effects

It can be shown by analysis that large-break LOCAs are not considered to be a serious PTS problem. This is because in the unlikely event of a large break in the primary system, high pressure cannot be maintained in the reactor pressure vessel. Small-break LOCAs (less than two inches equivalent diameter) also are not a PTS problem because breaks in this size range result in cooldown rates of less than 100°F per hour. Such transients do not cause large thermal stresses. Breaks in the range of two up to possibly as large as six inches are of concern. These breaks are capable of removing all of the decay heat generated in the core and do not require or establish natural circulation for decay heat removal. Mixing of ECCS water in the downcomer is minimized in this case. (See Section K.3.) In addition, reactor system pressure can remain relatively high (~1200 psi) for the considerable amount of time required to uncover the break (i.e., steam discharge out of the break), or repressurization can occur after initiation of the break for some plants with high head HPI pumps. This scenario, loss of natural circulation with high pressure, at present appears to be the one most likely to benefit from heating ECCS water in order to reduce the PTS problem. For secondary side events (e.g., main steam line breaks), rapid cooldown and depressurization of the primary system can occur. ECCS actuation will repressurize the primary system. However, since there is no primary system LOCA, only a limited volume of ECCS water will be injected into the primary system by the operator to make up for shrinkage due to cooldown. Therefore, as far as PTS is concerned, the cooldown is not affected as much by ECCS injection as by primary to secondary heat transfer. However, for certain secondary side events (e.g., steam and feedline breaks) including steam generator tube rupture, interruption of circulation and consequent temperature transients that could be influenced by ECCS water temperature could be possible. At this time, sufficient analysis has not been done, and conclusions regarding these events would be premature.

K.2 Plants Which Have Raised Their ECCS Water Temperature

Several plants have the capability to heat the ECCS water. Connecticut Yankee heats the refueling water storage tank (RWST) to 50°F in the winter to prevent water in the outdoor tank from freezing. Maine Yankee has a technical specification to maintain the RWST at a minimum temperature of 40°F. The water currently is heated no higher than 80°F. Yankee Rowe is the only U.S. plant that heats its ECCS water substantially above normal ambient temperatures, even in the summer. The safety injection tank water temperature is maintained at 120°F (130°F maximum) to minimize any PTS problem. A review has been conducted by Yankee Atomic Electric Company to ensure that the increased water temperature would not adversely impact postulated accidents.

The Loviisa plant in Finland maintains the ECCS water temperature between a minimum of 113°F and a maximum of 140°F. One of the reasons for this is because the low pressure ECCS system injects through nozzles directly into the reactor vessel. There is no mixing in the cold leg, so the ECCS water is heated to minimize the thermal shock.

K.3 Mixing of ECCS Water

Mixing of ECCS water with water in the reactor vessel has been and continues to be evaluated through analysis and experimentation (Ref. K.1-K.3). As long as adequate reactor coolant flow is maintained, good mixing of ECCS water in the cold leg downcomer is expected, and heating the ECCS water is expected to be of little benefit from a PTS standpoint. In the event that loop flow stagnated, the degree of mixing of ECCS water injected into the cold leg is less certain. If mixing were minimal, colder ECCS water could contact the reactor vessel wall, and therefore, heating the ECCS water would be beneficial in reducing thermal stresses.

APPENDIX L

NONDESTRUCTIVE EVALUATION METHODS

L.1 Detectability of Underclad Cracks

In order to have confidence that an inservice inspection (ISI) could detect near surface flaws in reactor pressure vessels that would be of interest in a pressurized thermal shock incident, it is necessary to demonstrate high probabilities of detection for 6.0 mm and larger cracks. Cracks of interest are both parallel and perpendicular to the clad lay. Weld defects within the first 25 mm as well as cracks resulting from clad deposition are of interest. European techniques using longitudinal waves are generally accepted as providing optimum detection results and have been shown to be effective in detecting 3.0 mm or smaller underclad cracks under the more ideal conditions of smooth clad and cracks predominantly perpendicular to the clad lay found in European pressure vessels. Most circumferential welds in U.S. pressure vessels have been clad using the manual metal arc (MMA) process. This welding process creates rough and noisy inspection conditions that inhibit inspection effectiveness. The NRC has, therefore, requested the Pacific Northwest Laboratory (PNL) to evaluate the reliability and effectiveness of these techniques for inspecting U.S. vessels. (See Section L.2.) Results of tests show that light grinding of the clad surface (specifically improving the surface roughness by a factor of 2, from 0.012 in. RMS to 0.006 in. RMS) improves the crack detectability confidence level from low to very high.

Further work is planned to refine the measurement methods for clad conditions, develop appropriate calibration methods, determine crack detection probabilities for various inspection techniques, and to establish performance of techniques for crack sizing. Hence, the surface roughness and cladding noise under field conditions could be quantified, a criteria established for determining if the cladding conditions permit a valid inspection to be performed, and a procedure given for an effective inspection.

L.2 Influence of Improved NDE Techniques

PNL has developed estimates to predict the influence of improved vessel examination techniques on vessel failure and allowable RT_{NDT}.

Table L-1 summarizes the results of this investigation. Using "best estimates" on probability of flaw detection, we have attempted to provide bounds for adjustments in allowable RT_{NDT} to reflect the benefit of optimized vessel inspection techniques. Table L.1 shows that the probability of flaw detection using optimized techniques varies from 50 to 95%, depending on clad type and surface finish. The corresponding benefit from inspection expressed as an increase in allowable RT_{NDT} varies from 10 to 33°F. In addition, we have provided supporting material for fracture mechanics and NDE in Sections L.2.1 and L.2.2 that indicate methodology used to derive Table L-1.

L.2.1 Fracture Implications of Improved Inservice Inspection

The results of probabilistic fracture mechanics calculations were available to PNL from the work of Mr. J. Strosnider of NRC (see Appendix H). These results were used to estimate an allowable increase in RT_{NDT} which could be justified on the basis of the estimated probability of crack detection for inservice inspection (ISI).

Figure L-1 shows trends of the NRC results for failure probability as a function of RT_{NDT}. Results for the NRC cooling rate curves for parameters $\beta = 0.051$, 0.15, and 0.50 are shown along with other results for the temperature/pressure curves of the Rancho Seco transient. It was assumed that the range of interest was a failure rate of 10^{-4} given the occurrence of a transient. In Figure L-1, Po is the probability of failure at a given transient and RT_{NDT}, and P is the probability of failure for the same transient but increased value of RT_{NDT}. All calculations here were based on the $\beta = 0.15$ cooling rate parameter.

Investigations of the mixing phenomena under stagnant loop flow conditions are underway in order to better quantify the degree of mixing.

K.4 Maximum ECCS Water Temperature

The maximum heating that could be allowed without causing other problems with ECCS operation has not been calculated. The impact on containment sprays, pump net positive suction head, and ECCS performance are examples of factors that could limit the water temperature. Evaluations such as these would have to be done on a plant-specific basis.

REFERENCES

F.1. Rothe, P. H., and W. D. Marsher, Fluid and Thermal Mixing in a Model Cold Leg and Downcomer with Vent Valve Flow, EPRI NP-227, March 1982.

F.2. Rothe, P. H., and M. F. Ackerson, Fluid and Thermal Mixing in a Model Cold Leg and Downcomer with Loop Flow, EPRI NP-2312, April 1982.

F.3. Levy, S., An Appropriate Prediction of Heat Transfer During Pressurized Thermal Shock, SLI-8213, June 1982.

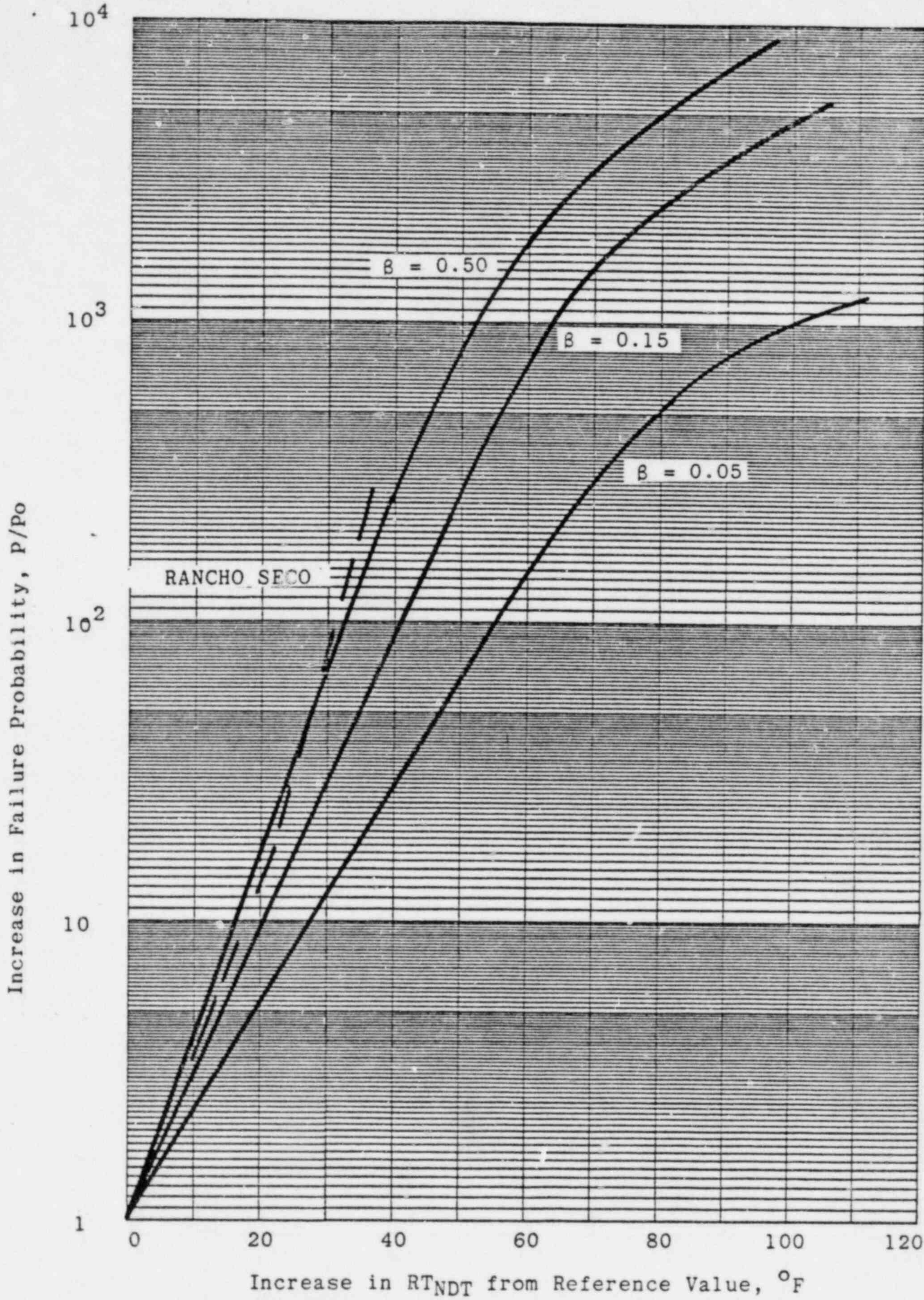
TABLE L-1

ESTIMATED DETECTABILITY OF UNDERCLAD CRACKS AND ESTIMATED INCREASES IN ALLOWABLE RT_{NDT}

CLAD	FINISH	FLAW DIRECTION WITH RESPECT TO CLAD	PROBABILITY OF DETECTION	FACTOR OF IMPROVEMENT (1) IN RELIABILITY	ALLOWABLE INCREASE IN RT_{NDT}, °F
Strip	Smooth	Perpendicular and Parallel	95%	20 to 40	27 to 33
Single Wire Strip	Smooth Unground	Perpendicular Perpendicular	85%, 0.5"-1.0" Flaw 90%, 1.0" or Greater Flaw	7.4 to 14.8	17 to 24
Single Wire Strip	Smooth Unground	Parallel Parallel			
Manual	Ground	Perpendicular and Parallel	75%, 0.5"-1.00" Flaw	4.3 to 8.6	13 to 19
Single Wire	Unground	Perpendicular and Parallel	80%, 1.0" or Greater Flaw		
Manual	Unground	Perpendicular and Parallel	50%, 0.5"-1.0" Flaw 75%, 1.0" or Greater Flaw	2.8 to 5.6	10 to 15

(1) Factor of Improvement = Probability of Failure without Inspection/Probability of Failure with Inspection.

Lower bound assumes flaws are isolated and independent occurrences. Upper bound assumes possible occurrence of multiple flaws in a given weld.



L-1
Figure 1. Relationship Between Failure Probability and RT_{NDT} as Indicated by NRC Analysis of Pressureized Thermal Shock of Reactor Vessels.

2-5

TABLE L-2

ESTIMATE OF FAILURE PROBABILITY WITH AND WITHOUT INSERVICE INSPECTION

				Failure Probability	
A	P(A)	P_{ND}	P(F/A)	$P(A) \cdot P(F/A)$ (without ISI)	$P(A) \cdot P_{ND}$ (with ISI)
0.125	8.3×10^{-1}	0.5	0	0	0
0.25	1.6×10^{-1}	0.05	1.5×10^{-4}	2.4×10^{-5}	1.2×10^{-6}
0.50	4.2×10^{-3}	0.5	1.0×10^{-2}	4.2×10^{-5}	2.1×10^{-6}
1.0	4.1×10^{-4}	0.05	5.4×10^{-2}	2.2×10^{-5}	1.1×10^{-6}
1.5	1.3×10^{-4}	0.05	5.6×10^{-2}	7.3×10^{-6}	3.6×10^{-7}
2.0	4.2×10^{-5}	0.05	4.5×10^{-2}	1.9×10^{-6}	9.5×10^{-8}
2.5	1.3×10^{-5}	0.05	-	-	-
3.0	5.0×10^{-6}	0.05	-	-	-
3.5	3.3×10^{-6}	0.05	-	-	-
				$Po(F) = 9.7 \times 10^{-5}$	$P(F) = 4.8 \times 10^{-6}$

Notes: (1) Based on data from status report by Jack Strosnider on "Failure Probability of a RPV Subject to Pressurized Thermal Shock," March 5, 1982

(2) For "Rancho Seco Transient Reference Case," mean copper = 0.34, mean RT_{NDT} = 0.0 and mean fluence = 3.0×10^{19}

(3) Probability of flaw nondetection (P_{ND}) for smooth strip clad

In PNL's calculations, the decrease in vessel failure probability due to ISI was first estimated. A trade-off between this decrease with an offsetting increase in failure probability due to relaxation in RT_{NDT} requirements was then performed. Table L-2 illustrates the estimate of failure probability as a function of probability of nondetection of a flaw (P_{ND}). In Table L-2:

A = Flaw depth

P(A) = Probability of a flaw of depth A in the critical weld

P(F/A) = Probability of failure for the Rancho Seco transient given the presence of a flaw of depth A

$P_{ND}(A)$ = Probability of not detecting a flaw of depth A based on PNL estimates

P(A)·P(F/A) = Probability of failure without ISI given the occurrence of the Rancho Seco transient

$P(A) \cdot P(F/A) \cdot P_{ND}$ = Probability of failure with ISI given the occurrence of the Rancho Seco transient

Table L-2 used the best detection capability corresponding the more favorable conditions of PNL's flaw detection studies. Results for other inspection conditions are given in Table L-1. The radio of failure probabilities in Table L-2 was 20:1 for the ISI case versus the no ISI case. Turning to Figure L-1, an increase in RT_{NDT} of 27°F will give an offsetting 20:1 factor in failure probability. Therefore, it is estimated for this particular example that the allowable RT_{NDT} can be increased by 20°F with no net increase in failure probability provided that no inservice inspection is performed.

It is recognized that the flaw size distribution in the NRC probabilistic analyses is subject to considerable uncertainties. Therefore, the estimated flaw size distribution as modified by ISI is subject to the same uncertainties. However, the relative improvement in reliability due to ISI is believed to be significantly more accurate than the absolute values of failure probability.

The upper bound estimate of the allowable increases in RT_{NDT} is an attempt to consider the statistical nature of underclad cracks. Evidence suggests that one can expect either no cracks at all or a large number of cracks. Given a large number of cracks is indeed very small ($P_{ND}\eta = 0.05^{10} = 10^{-13}$), thus, one can arrive at vastly different conclusions regarding the benefits of ISI, depending on the assumption on the stochastic structure of the flaw distribution. The upper bound estimate as shown in Table L-1 on the benefit of ISI conservatively assumes that half the flaws in vessels are random occurrences and that the remaining flaws occur in groups so to be readily detectable. The assumption that all flaws are random occurrences will tend to greatly underestimate the potential benefits of ISI. On the other hand, it is unreasonable to assume that random flaws will not occur, since one can be led to accept any level of embrittlement in a vessel provided that an ISI reveals no flaws.

L.2 Flaw Detectability Measurements

Flaw detectability experiments have been carried out on strip clad, single wire sub arc clad, and manual clad. Both ground and unground surfaces were evaluated. The test blocks used for this evaluation were: a 750-mm-dia. strip clad pressurizer dropout, two 600-mm square blocks with strip and single wire clad with one side ground and the other as welded*, two small blocks with ground and unground manual clad. The pressurizer dropout contained through clad notches as well as actual thermal fatigue underclad cracks. The two EPRI blocks contained unclad notches and the manual clad samples contained two reference reflectors for evaluation of general noise level. The measurements reported here were taken using a 2-MHz dual beam longitudinal (SEL) 70° transducer, with 10- by 15-mm elements and focal cross over point of 17 mm. This unit was considered optimum for the clad conditions and thicknesses (6 to 9 mm) tested. All measurements were performed manually.

The results of signal amplitudes compared to the signal amplitude of a 3 mm flat bottom reference reflector are shown in Table L-3. In addition, a blind test was conducted. This blind test used the pressurizer dropout sample that contained nine actual underclad cracks generated by a thermal fatigue process. The

*Access to these two samples was made possible through J. R. Quinn, Electric Power Research Institute (EPRI), Palo Alto, CA.

cracks were oriented both parallel and perpendicular to the direction of the cladding. The cracks ranged in depth from 0.25 to 0.75 inch through the wall. Although none of the three operators had prior knowledge of crack location, each operator detected every crack. The probability of detection data reported in Table L-1 are estimates based on an optimized inspection system, our flaw amplitude measurement and our blind test.

TABLE L-3

FLAW AMPLITUDE RESPONSE

SENSITIVITY STANDARD: 3MM FLAT BOTTOM REFERENCE REFLECTOR

SAMPLE TYPE	FLAW DEPTH RANGE	FLAW RESPONSE RANGE (+) GREATER REFERENCE REFLECTOR
Ground; Strip Clad; Underclad Notch	5mm to 18mm	0 to +9dB
Unground; Strip Clad; Underclad Notch	5mm to 18mm	0 to +8dB
Ground, Single Wire; Underclad Notch	5mm to 18mm	-1 to +10dB
Unground; Single Wire; Underclad Notch	5mm to 18mm	-1 to +12dB
Ground; Strip Clad Pressuirzer Dropout Underclad Cracks	5mm to 18mm	0 to +11 dB

APPENDIX M

INSITU ANNEALING

Annealing of the beltline region of reactor vessels is a potential remedial measure for the PTS problem for vessels that have suffered considerable radiation embrittlement.

Time-Temperature Effects on Recovery of Properties

There is a fairly good experimental basis for choosing the annealing temperature and time. From the Naval Research Laboratory, research funded by the NRC has revealed the effects of annealing at 650°F and 750°F and the effects of reirradiation and reannealing (Ref. M.1-M.3). Research at Westinghouse funded by the Electric Power Research Institute (EPRI) has revealed the effects of annealing at temperatures of 650, 700, 750, 800 and 850°F (Ref. M.4). As expected, there is a clear trend toward better recovery of properties at the higher temperatures and at longer times up to one week. In this discussion, therefore, we will assume that annealing would be done at 850°F for one week, and that the resulting recovery of fracture toughness properties would be about 80 percent.

Reirradiation Effects

With regard to the rate at which ΔRT_{NDT} increases upon reirradiation, the data are scattered and somewhat conflicting. The rate of reembrittlement should be as low as that just prior to annealing, and is almost certaily significantly lower than that at the start of life. Thus, a plant that annealed its vessel after, say, 8 EFPY should expect much more than 8 additional EFPY before reaching the same ΔRT_{NDT}. Obviously, a better estimate of the reirradiation rate is desired for economic considerations before undertaking annealing; but for purposes of safe operation in later years, there will be additional information from test reactor programs and from plant surveillance data.

One technical question that has yet to be thoroughly investigated is the verification test program for a specific plant, which will be required to measure the effects of the annealing operation and the reirradiation.

Appendix G of 10 CFR 50 requires that the degree of recovery be measured "...by testing additional specimens that have been withdrawn from the surveillance program capsules and that have been annealed under the same time-temperature conditions as those given the beltline material."

The specimens in most capsules have been irradiated substantially more than the vessel; hence, measurement of ΔRT_{NDT} for those specimens after annealing should give a conservative estimate of the condition of the vessel. Their use as a guide to the rate of reembrittlement is not well understood. One alternative is to test "reconstituted" Charpy specimens from earlier surveillance capsules, i.e., fabricate Charpy specimens by welding ends on the broken halves of specimens that have lower fluences because they were withdrawn from the vessel early in life. Another alternative is to irradiate archive material to the desired fluence in test reactors and then check the effects of annealing and reirradiation.

With regard to the feasibility of annealing, NRC staff has the results of the EPRI study (Ref. M4) and the (potential) advice of vessel fabricators who have experience in post-weld heat treatment after field fabrication and after repairs. The EPRI study developed a means of heating by electric resistance elements supported on a frame that would be lowered into the vessel before the water is removed. No insurmountable difficulties were reported, but many engineering details remain to be resolved

From the standpoint of risk, the main conern seems to be the potential for distortion of the vessel and the economic risks associated with problems in reinstallation of the core support structure and the closure head. At 850°F, some creep and relaxation could occur at regions where there are significant stresses caused by differential expansion during heatup and cooldown, by residual stresses, and by the stresses near the supports caused by the dead weight of the vessel. These problems have not been dealt with very carefully or completely as yet. From what has been done, it dos not appear that the piping would have to be separated from the vessel. Again, the experience of field fabricators of vessels must be tapped.

Other components that require study of the risks of annealing are the vessel insulation, the adjacent concrete and the supports. The movement of the vessel relative to the support when heated to 850°F will of course be greater than that at the design temperature of 650°F. Also, for those supports where the concrete is only a short distance below the vessel nozzle that must carry the load, the structural integrity of the concrete must not be impaired.

In conclusion, it appears that from the safety standpoint the benefits of annealing are quite clearcut and the risks are low. The risks of annealing are economic risks. There is, of course, a cost in man-rem and dollars if everything goes as planned. The largest uncertainty remains the economic and exposure risks associated with correction of distortion of the vessel or other damage if things do not go as planned.

REFERENCES

M.1. J. R. Hawthorne, "Status of Knowledge of Radiation Embrittlement in USA Reactor Pressure Vessel Steels," NUREG/CR-2511, NRC Memo Rept. 4737.

M.2. C. Z. Serpan, H. E. Watson, and J. R. Hawthorne, "Interaction of Neutron and Thermal Environmental Factors in the Embrittlement of Selected Structural Alloys for Advanced Reactor Applications," Nuclear Engineering and Design, V.II, 1970, pp. 368-380.

M.3. C. Z. Serpan and J. R. Hawthorne, "Yankee Reactor Pressure Vessel Surveillance: Notch Ductility Performance of Vessel Steel and Maximum Service Fluence Determined from Exposure During Cases 2, 3, and 4. ASME Journal of Basic Engineering, V. 89, 1967, pp. 897-910.

M.4. EPRI Thermal Annealing Program, Research Project 1021-1. Development of a Generic Procedure for Thermal Annealing on Embrittled Reactor Vessel Using a Dry Annealing Method. EP Interim Report July, 1982, T. R. Mager and R. D. Rishel, PN-2483. Only drafts of other parts of the final report were available as of August 15, 1982.

APPENDIX N

FUTURE CONFIRMATORY STUDIES

N.1 Introduction

The following issues relating to pressurized thermal shock require confirmatory study.

1. Applicability of Linear Elastic Fracture Mechanics (LEFM) for initiation, propagation and arrest for reactor pressure vessels subjected to a pressurized thermal shock scenario.

2. Effectiveness of Warm Prestress

3. Vessel failure under nonpressurized thermal shock conditions.

4. Behavior of small finite flaw when subject to PTS conditions.

5. Cladding-flaw interaction; bimetallic effects.

6. Irradiated cladding material and fracture properties.

7. Arrest on the upper shelf.

8. Postarrest performance for a deep crack in upper shelf material toughness.

9. Definition of margin when using RT_{NDT} to set fracture toughness curves.

10. Variation of through-wall fracture toughness degradation.

11. Validation of fracture toughness degradation as a function of fluence for ferritic welds.

12. Effect of trace elements (copper, nickel, phosphorus) on the embrittlement rate of RPV steels at reactor operating conditions.

13. Effectiveness of thermal annealing on fracture toughness recovery and reembrittlement rate.

14. Establishments of criteria and standards to be applied to any proposed, in situ thermal annealing of operating reactor vessels.

N.2 Summary of Prior Studies

Thick section pressure vessel materials have been characterized to form the basis for fracture toughness and crack growth data in the ASME B&P codes. Crack arrest methodology has been extensively evaluated and preliminary specimen designs developed. Methods of elastic-plastic fracture analysis have been developed and evaluated. Irradiation effects on pressure vessel plate, forging and weldments, including low-shelf thoughness weldments, have been studied using compact specimens up to 4 inches thick. Thirteen intermediate tests have been performed on nine vessels to validate methods of fracture-failure analyses, to demonstrate the capability of NDE methods and repair procedures in thick sections. Seven thermal shock (unpressurized) experiments have been performed on thick-section cylinders to demonstrate the applicability of LEFM in predictions of flaw behavior and to establish the applicability of small specimen toughness determinations in fracture analysis. Unique crack arrest data have been developed in these tests. Small scale stainless steel cladding tests have been performed to determine the influence of cladding on flaw development. Computer codes have been developed to evaluate fracture potential to define and quantify the principal variables that need to be considered in operating systems. The effect of trace elements, such as copper, nickel and phosphorus, on the embrittlement potential of commonly used reactor pressure vessel steels when subject to different levels of neutron bombardment has been determined. The effect of thermal annealing, at various temperatures on the fracture toughness recovery of neutron embrittlement steels has been defined and quantified. Elastic-plastic material fracture toughness testing procedures have been developed and elastic-plastic fracture data basis are being developed for unirradiated and irradiated reactor pressure vessel steels. Extensive participation with NRR, code writing bodies (ASME, ASTM), information dissemination through formal and informal exchanges, and international cooperative efforts have been maintained.

N.3 Present Programs Addressing Issues

The following programs are underway or are planned to address the issues identified in Section N.1. The numbers in parentheses refer to issues in Section N-1.

- ° Complete 3 dimensional finite element fracture computer codes [ORFLAW-3D and ORVIRT-3D] (4)(5)(6)
  - completion date: March 1983
- ° Complete evaluation of finite flaw behavior (4)
  -completion date: December 1982
- ° Complete development of unified LEFM-EPFM methodology considering all regimes of toughness (1)(2)(4)(5)(7)(8)
  -completion date: September 1983
- ° Complete testing of low-shelf weldments (8)(11)
  -completion date: December 1983
- ° Complete testing 1TCT irradiated specimens of present practice steel (11)
  -completion date: September 1983
- ° Complete irradiation of cladding material (6)
  -completion date: June 1983
- ° Complete testing of irradiated cladding material (6)
  -completion date: December 1984
- ° Complete material procurement for K_{Ic} (4T) study (7)(9)(11)
  -completion date: December 1983
- ° Complete irradiation for K_{Ic} (4T) study (7) (9)(11)
  -completion date: June 1985

- Complete testing for K_{Ic} (4T) study (7)(9)(11)
  -completion date: September 1985*

- Complete development of irradiated crack arrest data base (6)(7)(9)(10)(11)
  -completion date: September 1986

- Complete probabilistic fracture mechanics version of Computer Code OCA-2 (9)
  -completion date: September 1982

- Complete Thermal Shock Experiment TSE-7 (1)(3)(4)
  -completion date: March 1983

- Complete Thermal Shock Experiment TSE-8 (1)(3)(4)(5)
  -completion date: March 1984

- Complete Thermal Shock Experiment TSE-9 (1)(3)(4)(5)
  -completion date: March 1985

- Complete feasibility study and system design for Pressurized Thermal Shock Experiments (1)(2)(4)(5)(7)(9)
  -completion date: September 1982

- Complete PTSE facility construction checkout (1)(2)(4)(5)(7)(9)
  -completion date: April 1983

- Complete PSTE-1 (1)(2)(4)(5)(7)(9)
  -completion date: March 1983

*Interim data from testing program will be available at earlier dates in 1984 and 1985.

- Complete PSTE-2 (1)(2)(4)(5)(7)(9)
  -completion date: March 1984

- Complete development of crack arrest specimen and test procedures (1)(7)
  -completion date: March 1983

- Complete construction of capsules and begin irradiation of specimens in dose rate study. (11)(12)(13)
  -completion date: October 1982

- Complete dose rate study [show irradiation more closely simulating operating reactor experience] (11)(12)(13)
  -completion date: October 1985

- Complete testing of SSC-2 and PSF dosimetry specimens (10(11)(12)
  -completion date: March 1983

- Complete variable radiation sensitivity study (11)(12)
  -completion date: May 1983

- Complete high temperature (454°C) annealing study (13)(14)
  -completion date: March 1985

- Complete high temperature (454°C) annealing
  -reembrittlement rate study (12)(13)(14)
  -completion date: May 1986

- Complete study on the effectiveness of drop weight method of determining NDT and applicability of RT_{NDT} (9)
  -completion date: October 1984

- Complete program on System Requirements and Standards development for annealing of reactor pressure vessels (12)(13)(14)
  -completion date: October 1984

- Completion of testing irradiated material from KRB Block A pressure vessel wall (10)(11)(13)(14)
  -completion date: October 1983

N.4 Applicability of Research

The research program is integrated with the needs of NRC licensing in addressing the issue of pressure vessel integrity, both under normal and accident or upset operating conditions. Every element of the described program is based upon the need of NRR to define and quantify methods use for evaluating pressure vessel safety issues. Every element of the described program is reviewed frequently by NRR, from the U.S. industry, and American and International technical community for its appropriateness and applicability to known or anticipated safety issues. The timeliness of this ongoing research is such that approximately 70 percent of the issues to be resolved in PTS will be addressed and results obtained by the research effort within Fiscal Year 1983. The remaining 30 percent of the intially needed information should be available as follows: 20 percent in FY 1984, 5 percent in FY 1985, and 5 percent in FY 1986. The planned funding effort is as follows:

FY 1982	FY 1983	FY 1984	FY 1985	FY 1986
\$6,650K	\$6,850K	~\$6,000K	~\$6,000K	~\$6,000K

It should be noted that though most of the initial data will be developed as described above, a considerable confirmatory effort must be continued during the years 1983-1986 to ensure that the results obtained are statistically valid. Another reason for the extension of the program through FY 1986 is the time required to carry out an effective irradiation study.

The funding shown above is commited to four contracts through FY 1983 and thereafter to three contracts.

1. HSST program (ORNL)
2. Pressure Boundary Integrity for Water Reactor (ENSA)
3. Pressure Vessel Simulation (ORNL)
4. Systems Requirements for Annealing (EG&G/INEL, terminates FY 1983)

N.5 Confirmatory Studies on Fluence Trend Curves

N.5.1 Refinement of Chemistry and Fluence Factors

Immediate steps must be taken to scrub down the PWR surveillance data base and add data from BWR surveillance. Then a reanalysis is required to refine the copper and nickel terms and determine what the exponent on fluence should be, and whether it should be constant over the whole fluence range. Probably, test reactor data should be omitted until later when a time-temperature parameter is better understood. This represents a change in attitude from that on which Regulatory Guide 1.99 and the MPC trend curves are based. The change reflects the increasing number of surveillance reports in recent years, more than it reflects any increased suspicion that test reactor data and surveillance data are separate populations. There is now an EPRI data base in which the Charpy curves have been fitted by a hyperbolic tangent function and new values of Charpy shift calculated. These values must be compared with the existing data base, which was obtained from curves drawn by eye, and differences reconciled where possible. After these steps are taken; and the new regression analysis is performed, the results will be incorporated in Revision 2 of Regulatory Guide 1.99.

N.5.2 Long-Range Effort

There are two refinements that require further input from research efforts before incorporation in further revisions of Regulatory Guide 1.99. One is the change from fluence measured in terms of neutrons/square centimeter, ($E \geq 1$ MeV) to fluence measured in terms of a damage function that considers the effects of different energy spectra, probably displacements per atom (dpa). The other refinement to be expected is a time-temperature parameter that accounts for irradiation temperature and exposure time. Both refinements are needed to permit the inclusion of test reactor and surveillance data in the same data base with complete confidence that they belong in the same population.

N.5.3 ORNL Study

N.5.3.1 Objective and Scope

The objective of this study is to provide an independent probabilistic analysis of PTS at a representative B&W, CE, and W PWR. The results will estimate the likelihood of vessel cracking due to PTS, identify what is important (dominant sequences, important operator actions, etc.) and will identify major uncertainties. The results will also provide a comparison of the risk-reduction effectiveness of alernative corrective actions.

The scope of the study is limited to addressing the reliability of pressure vessel integrity and does not address the consequences of vessel failure. The study of the three plants, Oconee 1, Calvert Cliffs 1, and H. B. Robinson 2, will be plant specific. Extension of this study to a generic analysis of classes at plants is beyond the scope of this study.

The study will support resolution of USI A-49 in four ways:

(1) Confirm understanding of PTS; e.g., how likely is vessel failure? What are the important event sequences, operator actions, and control features? How effective are various proposed measures for reducing the likelihood of vessel failure?

(2) Improve methods for analyzing PTS.

(3) Provide a plant-specific analysis of PTS for three plants.

(4) Provide an improved basis for staff evaluation of plant-specific analyses.

N.5.3.2 Study Plan

The study will use a functional approach rather than a detailed component-by-component approach. Conceptually the plan is first to identify phenomena that could cause overcooling such as too much feedwater or too much ECC; second, to identify initiating events; and then to analyze the reliability of functions that prevent overcooling.

The study involves the following steps for each of the three plants.

First, the analysts (ORNL for probabililistic and fracture-mechanics analysis and LANL/INEL for thermal hydraulic analysis) obtain information on the plant and understand how the plant operates regarding overcooling transients.

Then ORNL performs an event-tree analysis to systematically delineate event sequences that could lead to overcooling and estimates the frequency of occurrences of these sequences.

About a dozen of these sequences are selected for detailed analysis by LANL using TRAC or by INEL using RELAP-5 to calculate temperature and pressure in the downcomer during the transient. These dozen cases are selected to cover a range of severity. Initially, in the Oconee study, both TRAC and RELAP-5 are used to compare and help check out the codes. Subsequently TRAC will be used to analyze Calvert Cliffs, and RELAP-5 will be used to analyze H. B. Robinson.

The method for assessing these TRAC and RELAP-5 models of specific plants (including secondary and control systems) is still being developed. Tentative plans are to calculate plant behavior during a transient such as a turbine trip and compare the results with plant data regarding behavior of turbine-bypass valves, feedwater flow, steam generator levels, reactor coolant temperatures, etc. The intent is to verify that the code behaves reasonably in transients of interest.

For each transient the coolant temperature and pressure calculated by TRAC or RELAP-5 will be used in a fracture mechanics calculation of the conditional probability of vessel failure given that transient occurs. Based on these results, ORNL will estimate the consequence to vessel integrity for each of the transient sequences in the event trees. Each of the sequences will then be sorted into one of a half dozen or so damage bins. These bins will be identified in terms of how many years the plant could operate before the transients in that bin could crack the vessel. Bins, for example, would be 0-5 yrs., 6-10 yrs., etc. The likelihood of vessel cracking will be added up for all the sequences in each bin to obtain the frequency of vessel-cracking vs. effective-full-power years. Dominant sequences will be apparent in the results.

N.5.4 Status and Schedule

The Oconee probabilistic study started in FY 1982, following a preliminary survey of available information in the Summer of 1981. The analysis is scheduled to be completed in January 1983, and the draft report in March 1983.

In July 1982, the owners of Calvert Cliffs and H. B. Robinson agreed to participate in the study. These analyses will begin in August and September 1982, respectively. The analyses should be completed in September 1983 with staff reports completed in November 1983.

APPENDIX O

SUMMARY OF ORNL FRACTURE-MECHANICS ANALYSIS FOR SEVERAL PWR RECORDED OCA TRANSIENTS*

Fracture-mechanics calculations were made recently for several PWR overcoooling accidents (OCAs) that have occurred since 1970, including the 1978 Rancho Seco transient (Ref. O-1). Information pertaining to these transients is presented in Table O-1 and Figures O-1 to O-6.

TABLE O-1 PWR OCA DATA[a]

Plant	Date of Accident	Vessel Dimensions (in.) Inner radius	Wall thickness	RT_{NDTo} (°F) Cir. weld	Long. weld
H. B. Robinson	4/28/70	78	9.31	-20	-20
H. B. Robinson	11/5/72				
H. B. Robinson	5/1/75				
Rancho Seco	3/25/78	86	8.5	b	+60
TMI-2	3/28/79	86	8.5	b	+20
Ginna	1/25/82	66	6.5	+20	c

[a]Data obtained from Nuclear Reactor Regulation, NRC, 6/16/82.

[b]Data not available.

[c]Forged vessel (no longitudinal welds).

Figures O-1 to O-6 describe the primary-system-pressure transient and the coolant-temperature transient in the cold leg upstream of the point where the emergency core coolant (ECC) is injected. Because of the location of the temperature measurement, the recorded temperatures are not necessarily accurate indications of the coolant temperatures in the downcomer. For instance, the injection of ECC would result in a lower temperature, and recirculation of core coolant through the vent valves in a B&W plant would result in higher temperatures. However, the fracture-mechanics calcuations have been made using the recorded temperatures in Figures O-1 to O-6 as downcomer temperatures.

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*Contribution by R. D. Cheverton, D. G. Bolls, and S. K. Iskandera of ORNL.

The curves in Figures 0-1 to were digitized for input purposes, using enough time steps to describ the curves accurately; essentially no smoothing of the curves was necessary. Thus, the analysis reflects the effect of nearly all of the irregularitie n the curves, except perhaps for the pressure curve in Figure 0-2. For this case it appears that the pressure dropped below 1700 psi but was not recorded. In the calculation, it was assumed for this particular case that the minimum pressure was 1700 psi.

The fracture-mechanics calculations were performed using OCA-II and the basic input data shown in Table 0-2.

In the process of making the fracture-mechanics calculations, a search was made for threshold values of the nil-ductility reference temperature at the inner surface (RT_{NDTS}) corresponding to incipient initiation (II). Results of the analysis are presented in the form of sets of critical-crack-depth curves for the threshold conditions (Figs. 0-7 to 0-18). A summary of the data is shown in Table 0-3.

In Figures 0-7 to 0-18, the existence of minimum points in the constant K_I curves indicates that the requisite conditions for warm prestressing (WPS) exist ($dK_i/dt < 0$). However, the existence of more than one minimum would indicate that K_I fluctuated with time, and under these circumstaces it is not clear that WPS would actually be effective. It was ignored, therefore.

Table O-2. Input Data for OCA Analyses

Parameter	Value	
Vessel dimensions[a]	See Table 1	
Cladding thickness, a/w	0.025	
Flaw Type	Long axial and continuous circumferential on inner surface and extending through cladding	
Range of flaw depths included in analysis, a/w	0.01-0.95	
Limits imposed on critical crack depths, a/w	0.025-0.15	
K_{IC} and K_{Ia}	ASME Section XI	
$(K_{Ia})_{max}$, ksi $\sqrt{in.}$	200	
$\Delta RT_{NDT} = f(Cu, Ni, F)$	$\alpha F^{0.27}$	
Fast neutron fluence (F)	$F = F_o \exp(-0.24a\ in.^{-1})$	
$\Delta RT_{NDT}(a)$[b]	$= \Delta RTNDT_s{}^{c} e^{-0.065a\ in.^{-1}}$	
ΔRT_{NDTs}, °F	≤ 500	
Fluid-film heat transfer coefficient (h_f), Btu/hr·ft²·°F	300[d]	
	Cladding	Base Material
Thermal conductivity (k), Btu/hr·ft·°F	10	24
Thermal coefficient of expansion (α), °F^{-1}	10×10^{-6}	8.04×10^{-6}
Modulus of elasticity (E) lbs/in.²	28×10^6	28×10^6
Specific heat (c_p), Btu/lb·°F	0.12	0.12
Density (ρ), lbs/ft³	489	489
Poisson's ratio	0.3	0.3

[a]Sets of K_I^* values were calculated for each set of dimensions.

[b]ΔRTNDT at the tip of the flaw.

[c]ΔRTNDT at the inner surface of the vessel.

[d]Corresponds to main circulating pumps off.

TABLE O-3 Results of OCA Analyses

Transient			Weld[a]	$RTNDT_s$,[b] °F	a_c,[d] in
Robinson	1970	broken loop	L	321 (F)	0.93
Robinson	1970	broken loop	C	351 (A)	0.93
Robinson	1972		L	381 (F)	1.4
Robinson	1972		C	>480	--
Robinson	1975	loop C	L	354 (F)	1.4
Robinson	1975	loop C	C	372 (A)	0.93
Robinson	1975	loop B	L	395 (F)	1.4
Robinson	1975	loop B	C	440 (A)	1.2
Rancho Seco	1978		L	295 (F)	1.3
TMI-2	1979	loop A	L	209 (F)	1.3
TMI-2	1979	loop B	L	225 (F)	1.3
Ginna	1982	loop B	C	378 (A)[c]	0.91

[a] L and C refer to longitudinal and circumferential.

[b] F and A in parentheses refer to failure and arrest.

[c] Small increase in $RTDNT_s$ would result in failure.

[d] Critical crack depth.

REFERENCES

R. D. Cheverton et al., "Thermal-Shock Investigations," Heavy Section Steel Technology Program Quart. Prog. Rep. for July-September 1981, ORNL/TM-8145, pp. 69-86.

FIGURE 0-1

~~FIG. 1~~

0

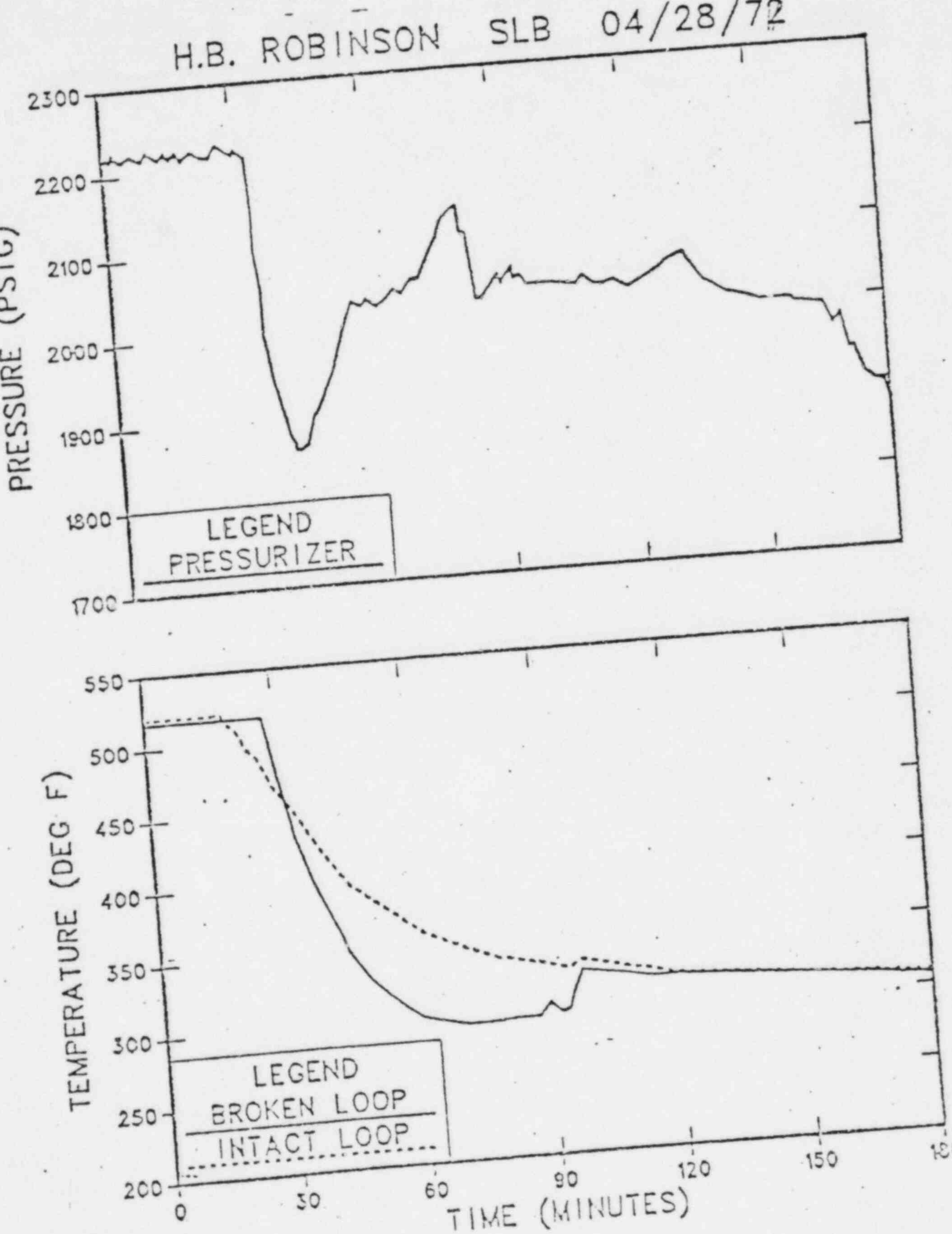


FIGURE O-2

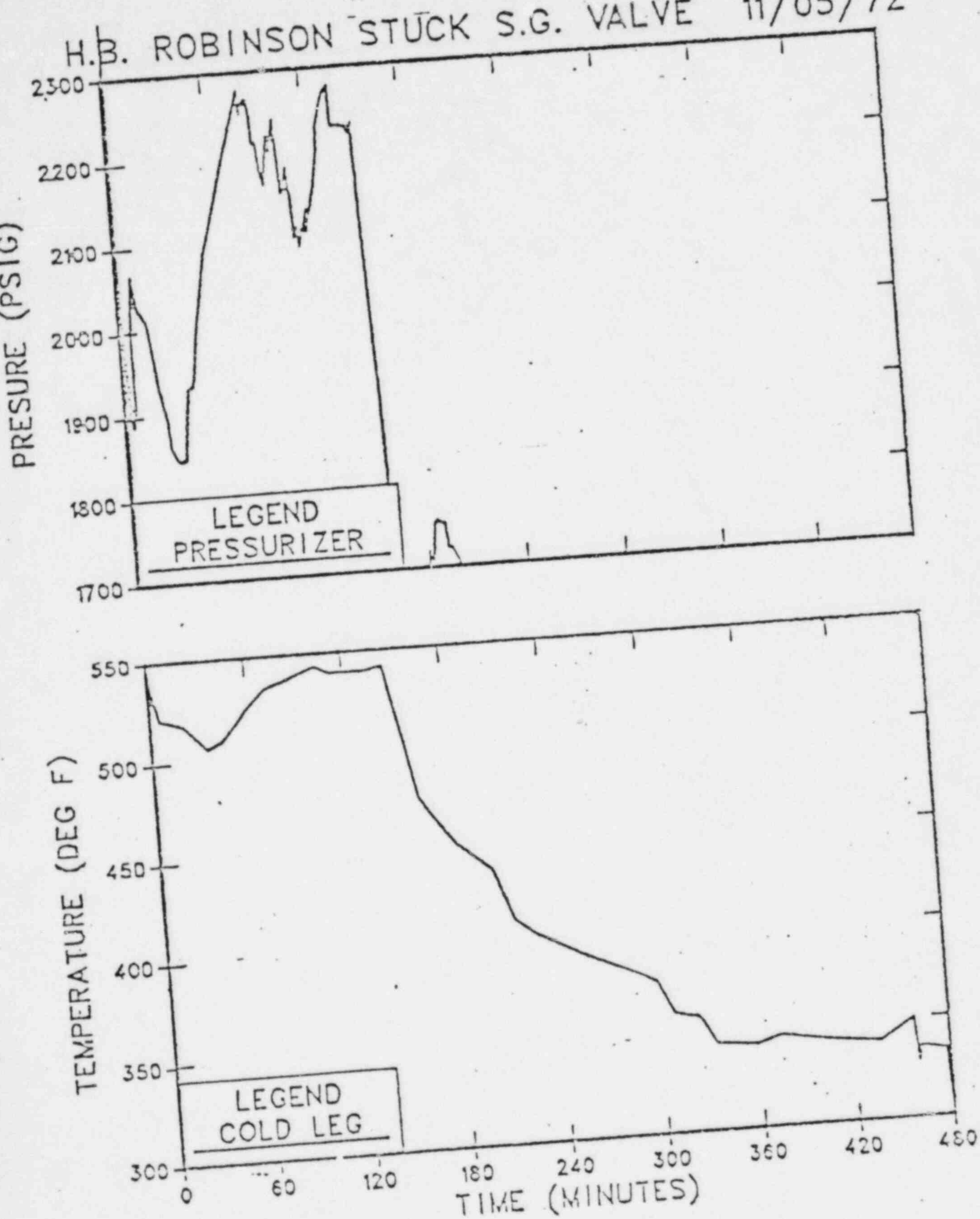

H.B. ROBINSON STUCK S.G. VALVE 11/05/72
PRESURE (PSIG)
2300
2200
2100
2000
1900
1800
1700
LEGEND
PRESSURIZER
TEMPERATURE (DEG F)
550
500
450
400
350
300
LEGEND
COLD LEG
0
60
120
180
240
300
360
420
480
TIME (MINUTES)

FIGURE 0-3

~~FIG. 3~~

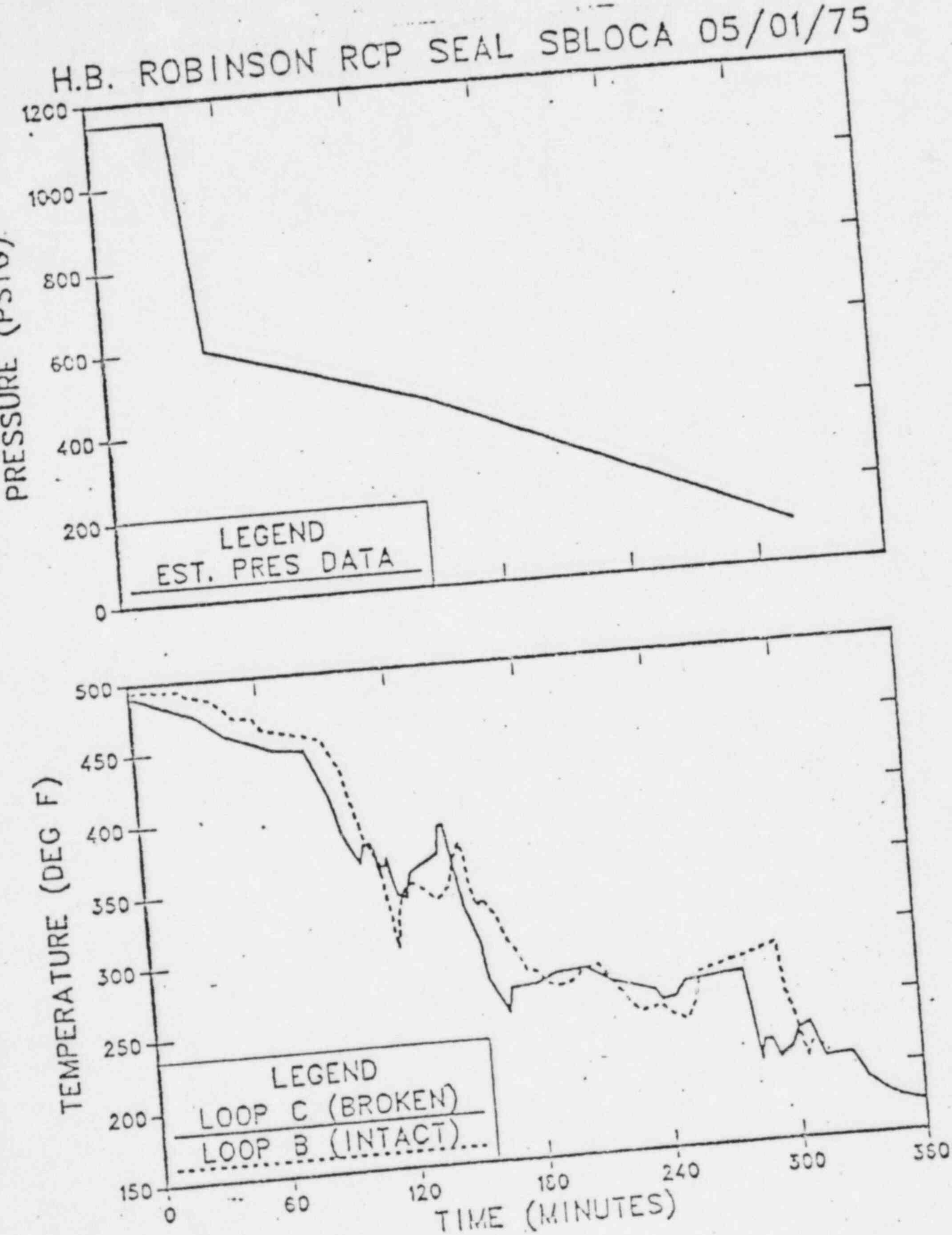


FIGURE 0-4 ~~FIG. 4~~

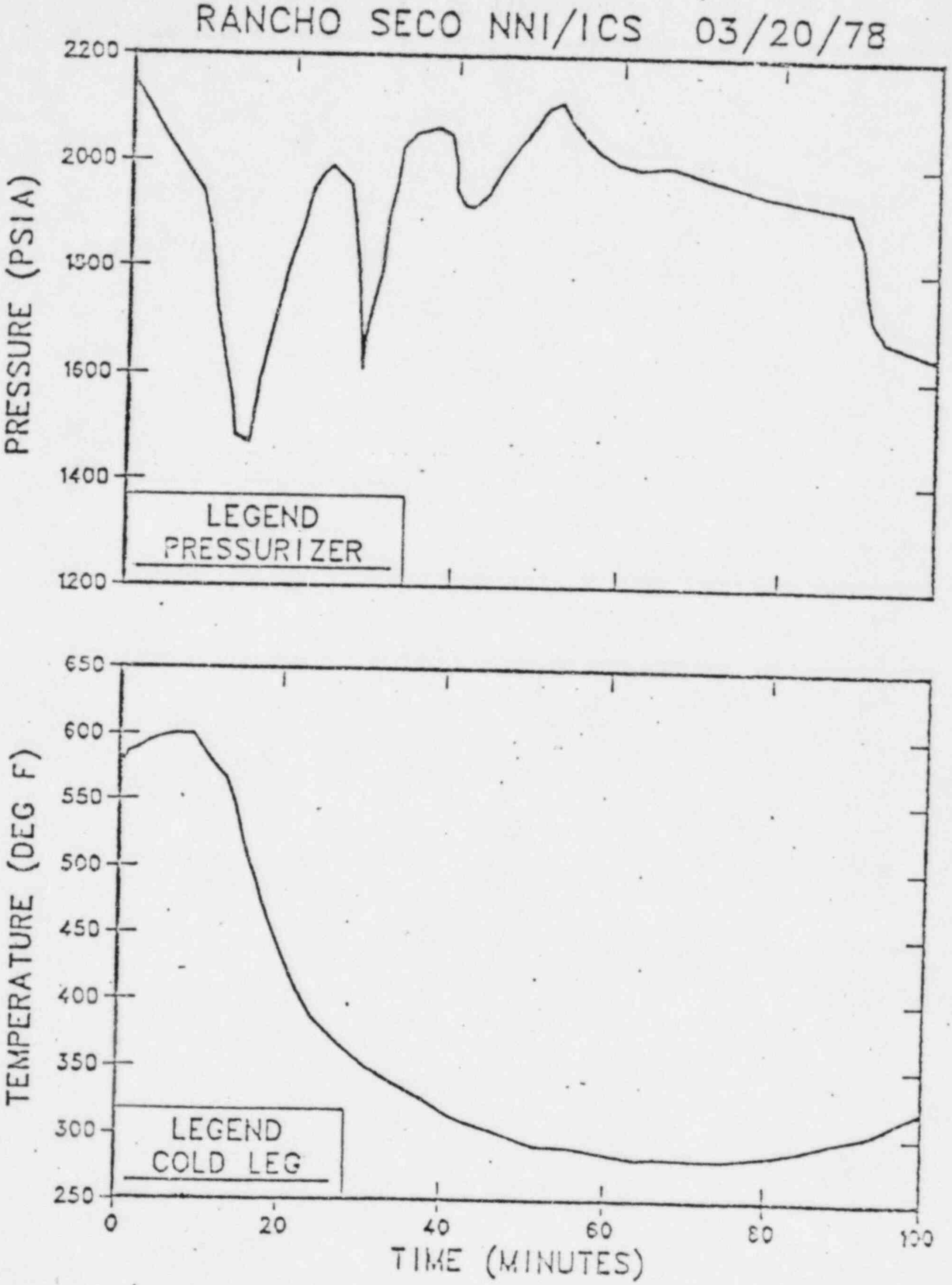


FIGURE 0-5

FIG. 5

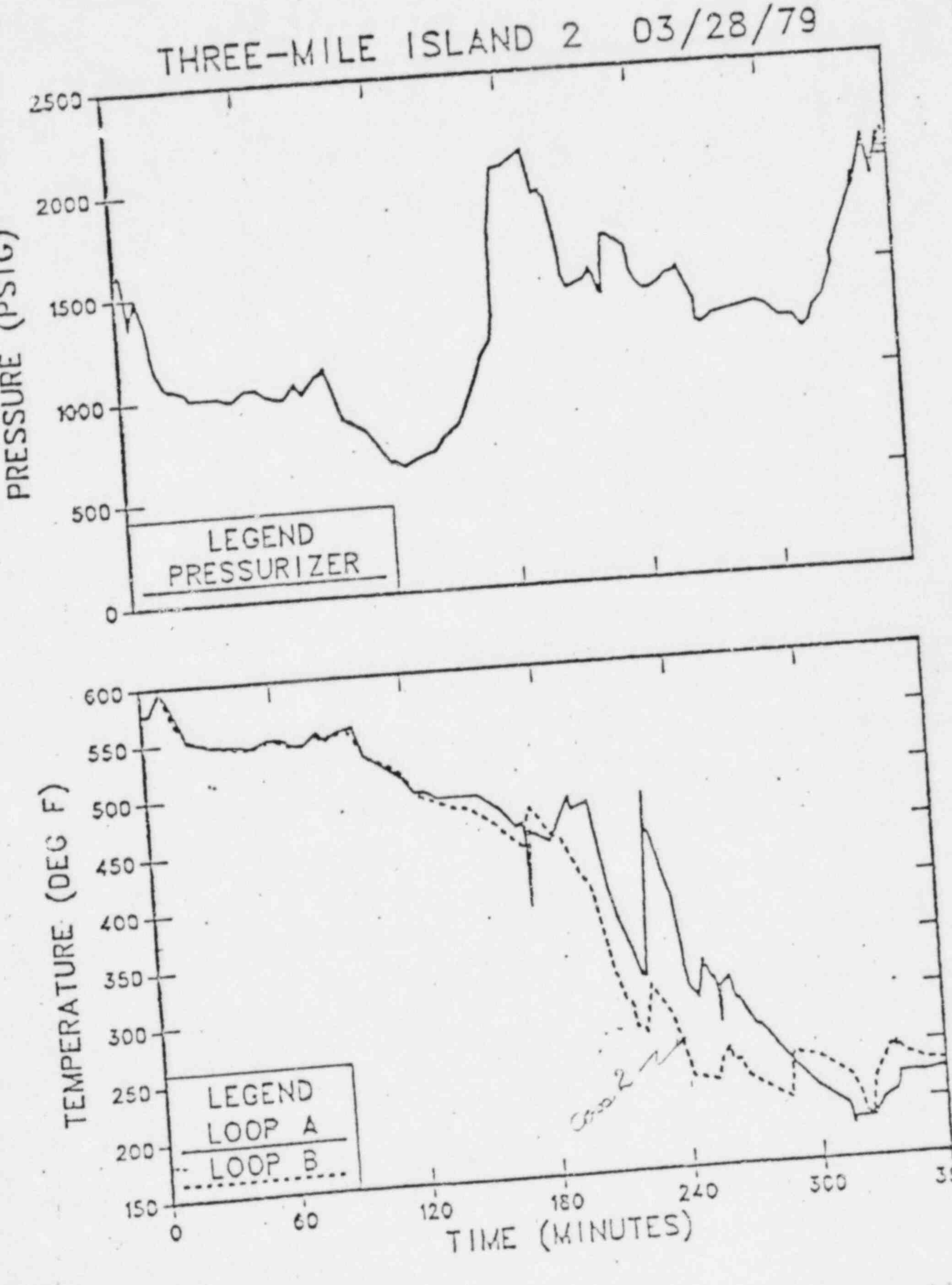


FIGURE 0-6

~~FIG. 6~~

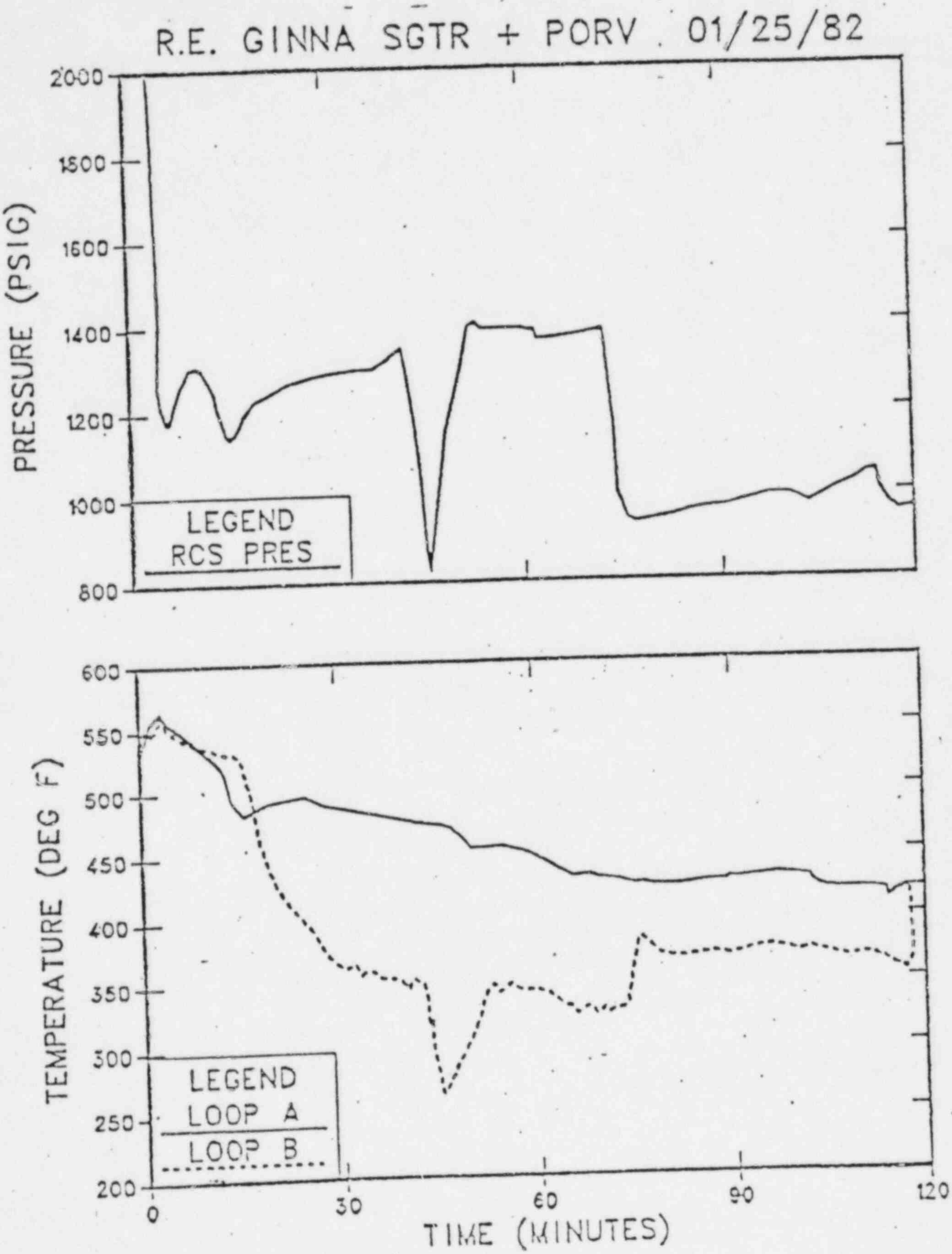


FIGURE 0-7

~~FIG. 7~~

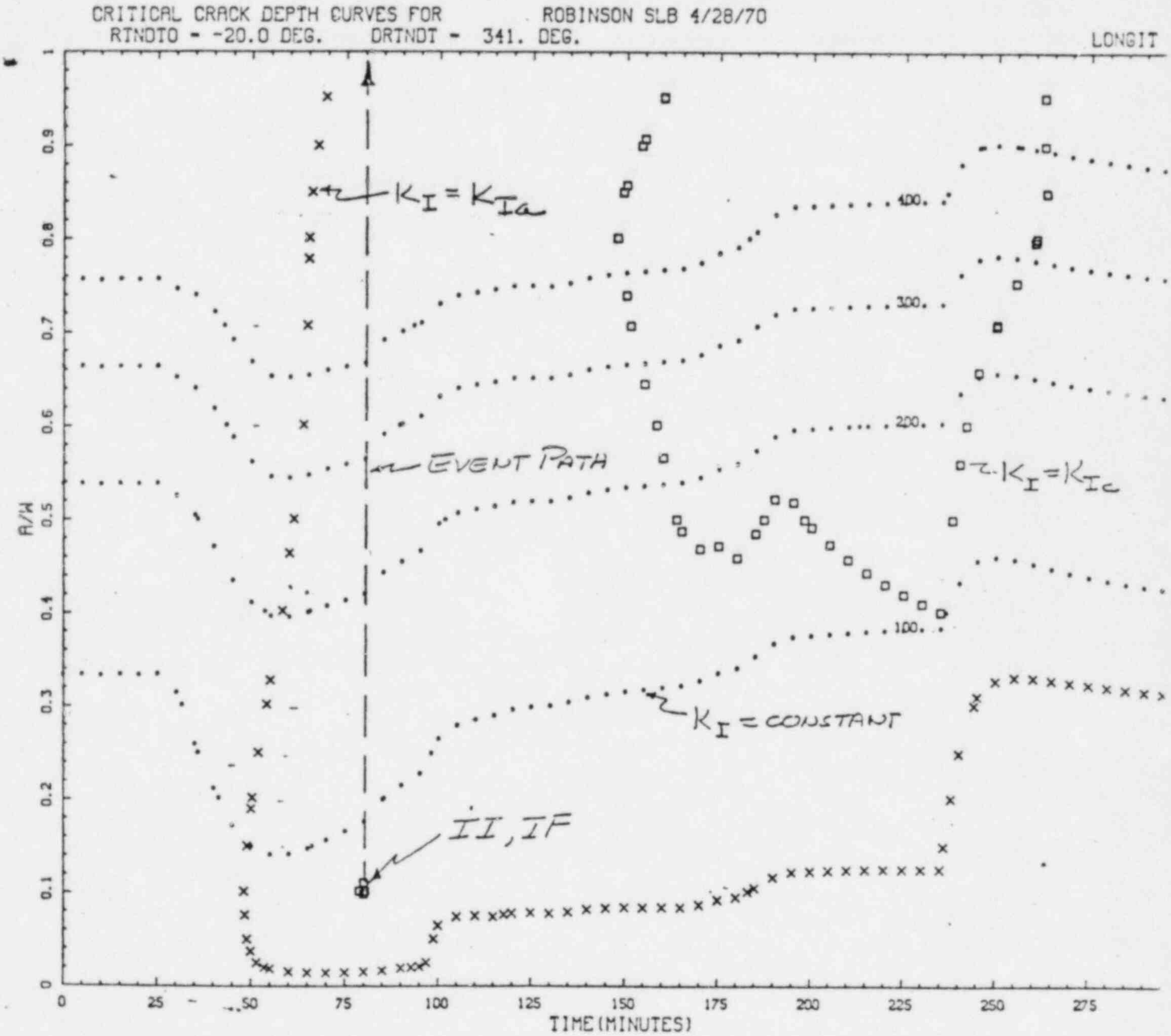


FIGURE 0-8

~~FIG. 8~~

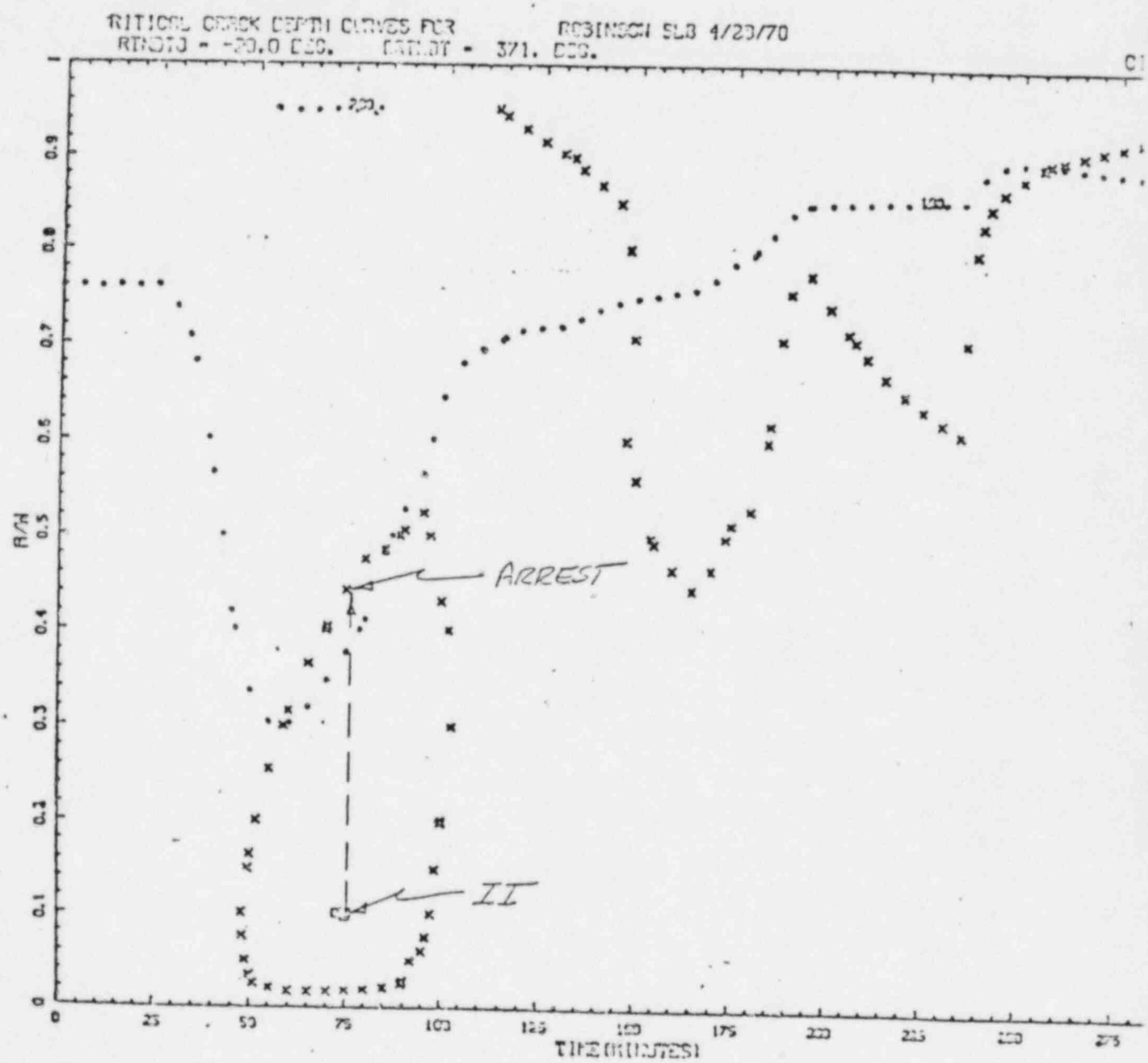


FIGURE 0-9

~~FIG. 9~~

CRITICAL CRACK DEPTH CURVES FOR
RTNDTO = -20.0 DEG. DRTNDT = 401. DEG.
ROBINSON STUCK S. G. VALVE 11/05/72

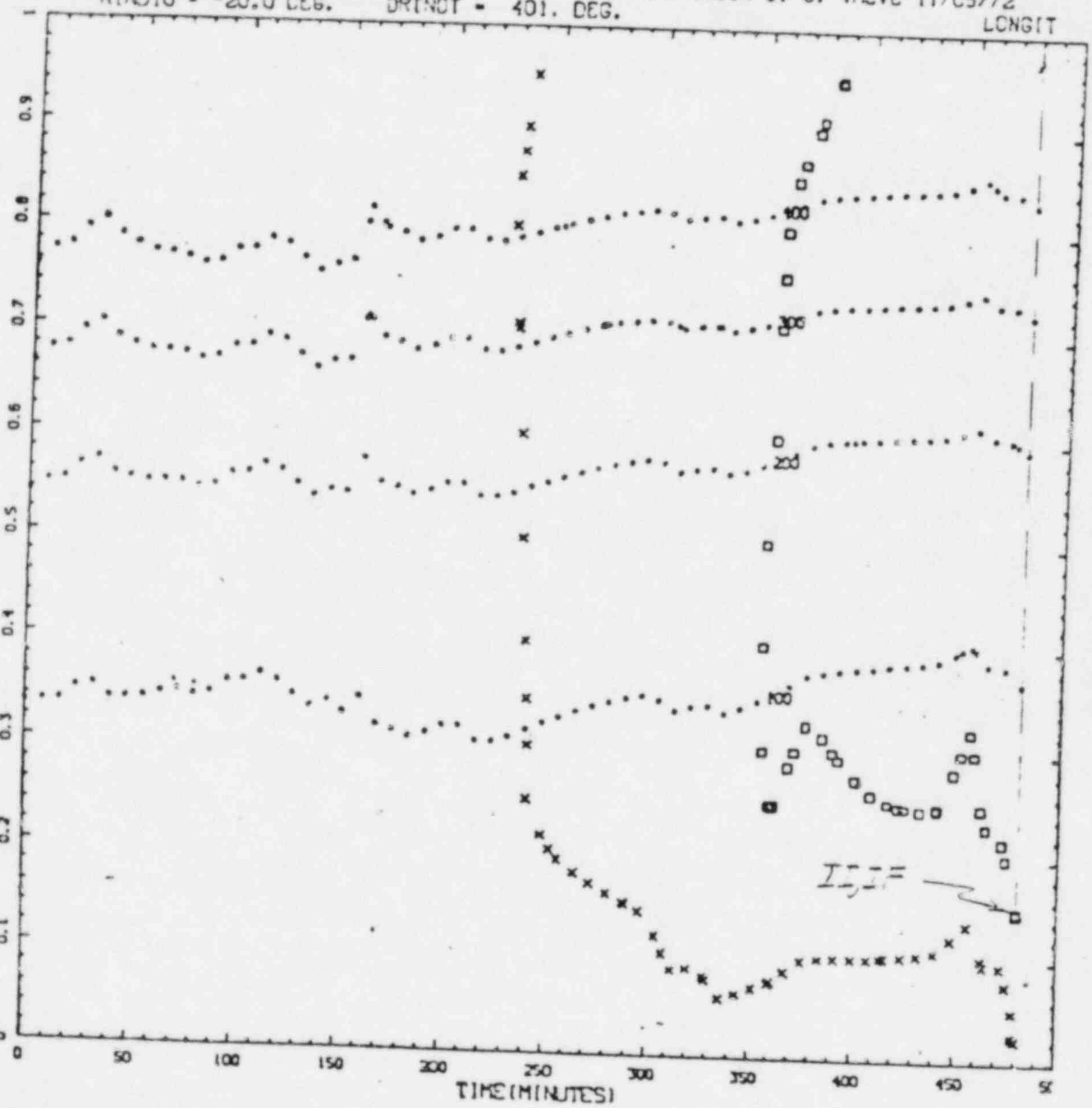


FIGURE 0-10

~~FIG. 10~~

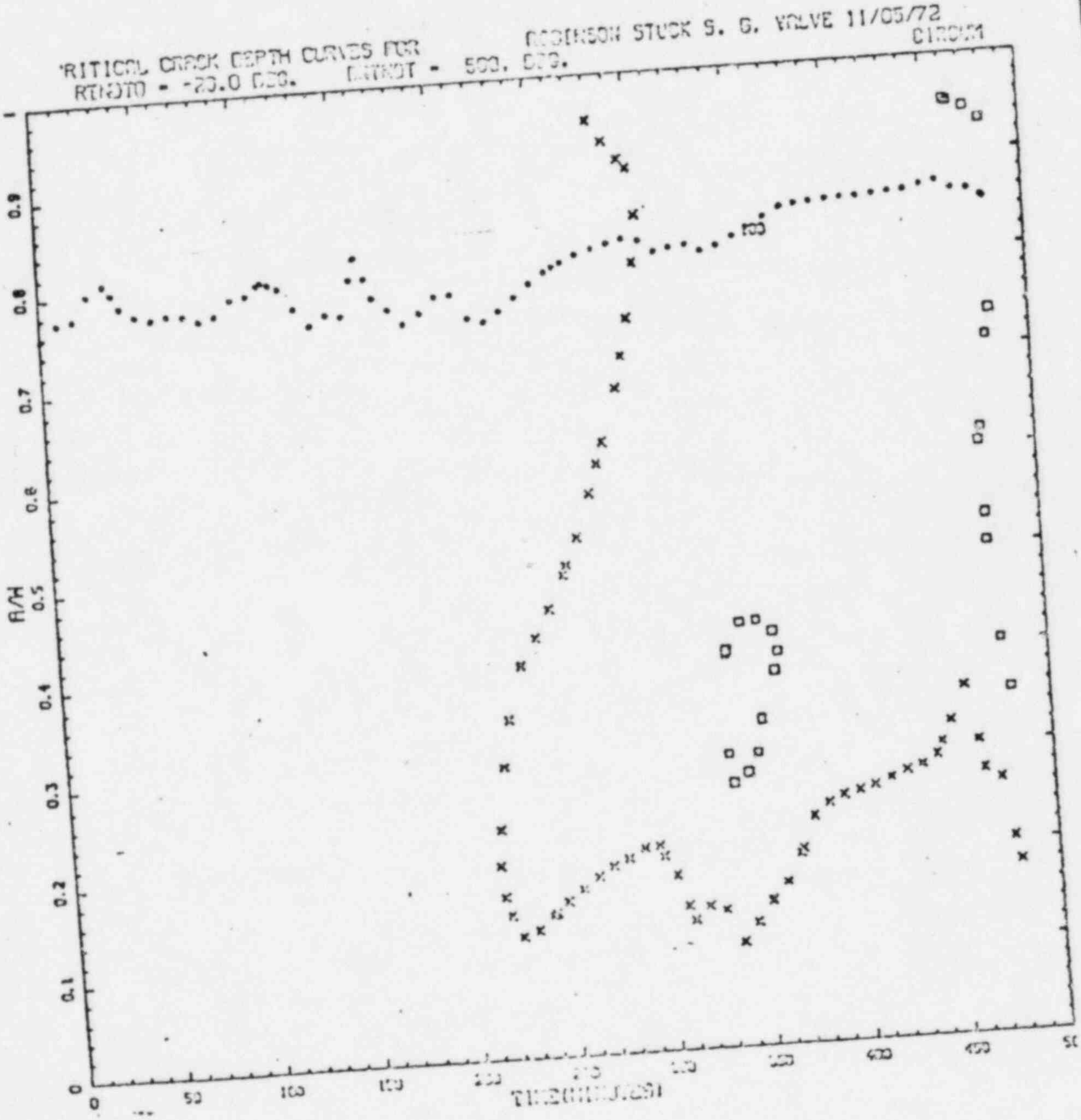


FIGURE 0-11

FIG. 11

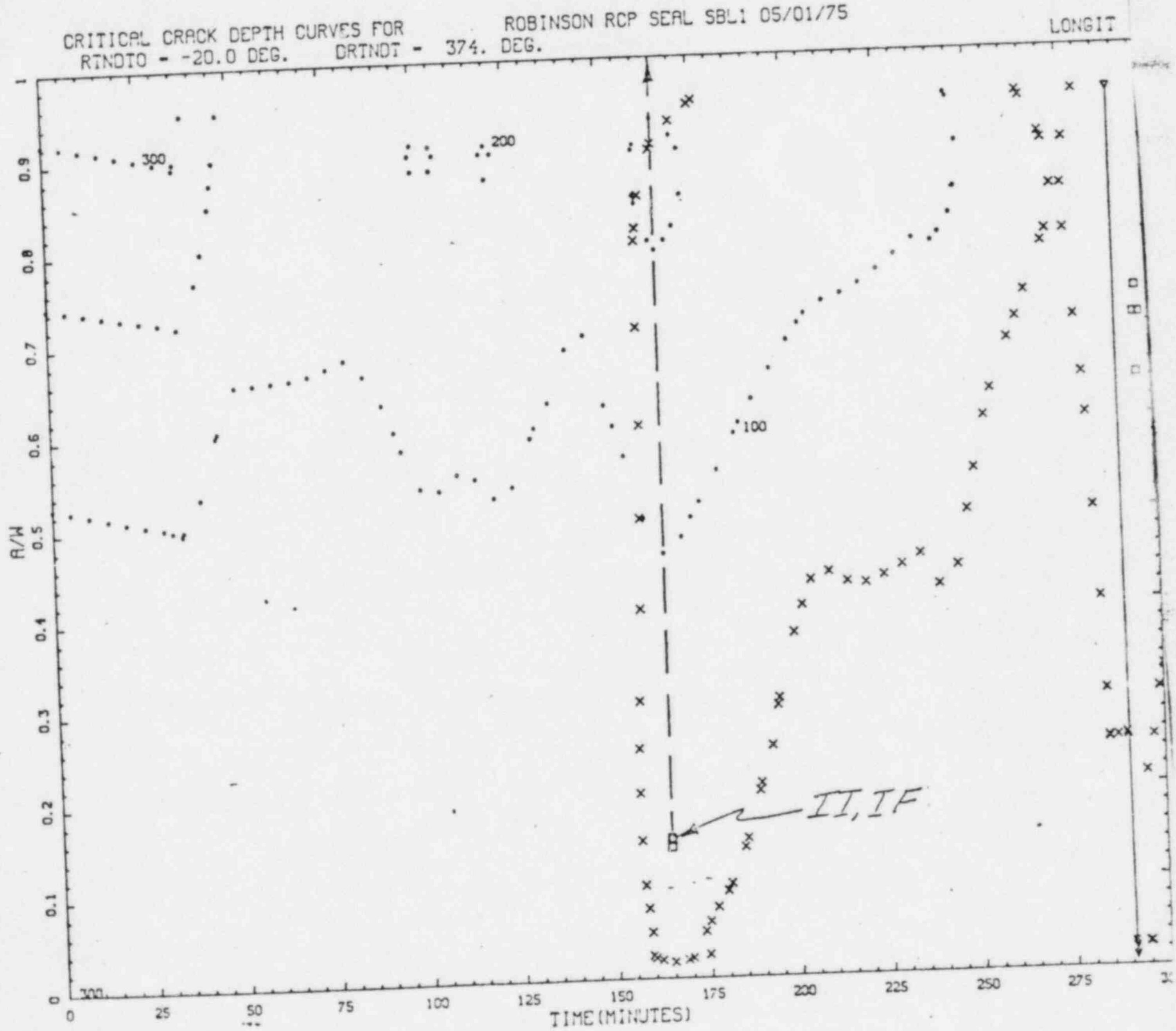


FIGURE 0-12

~~FIG. 12~~

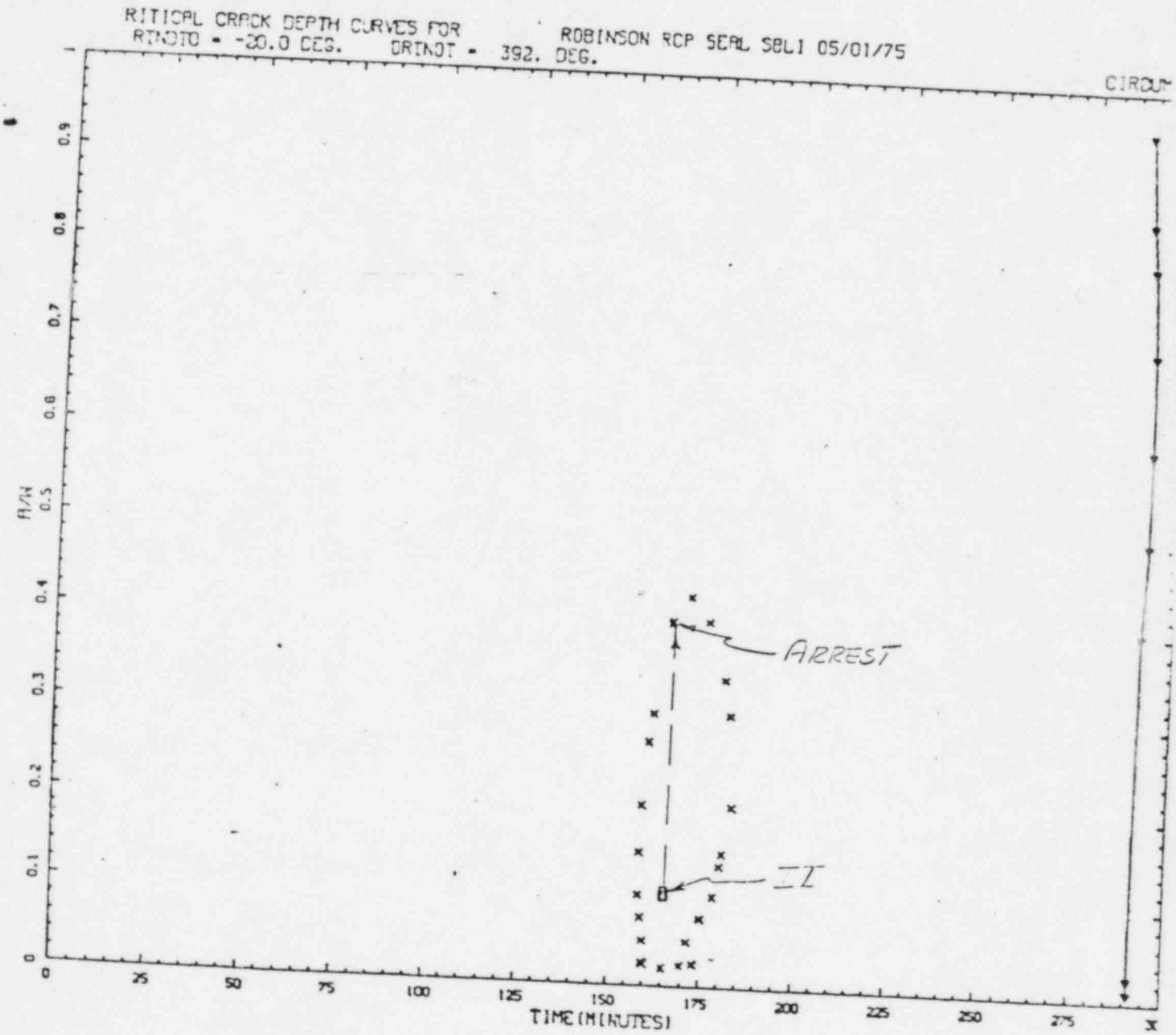


FIGURE 0-13

~~FIG. 13~~

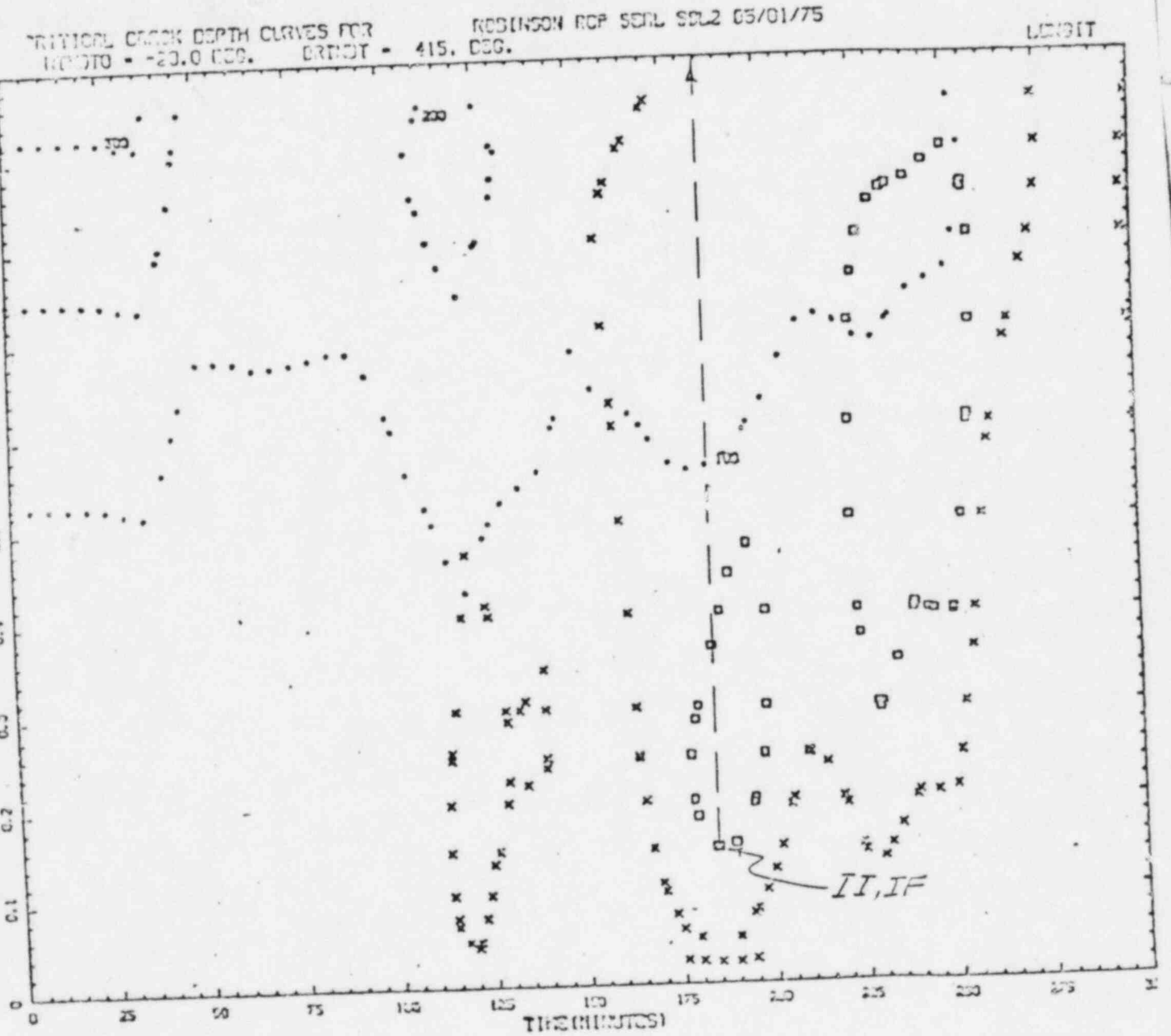


FIGURE 0-14

FIG. 14

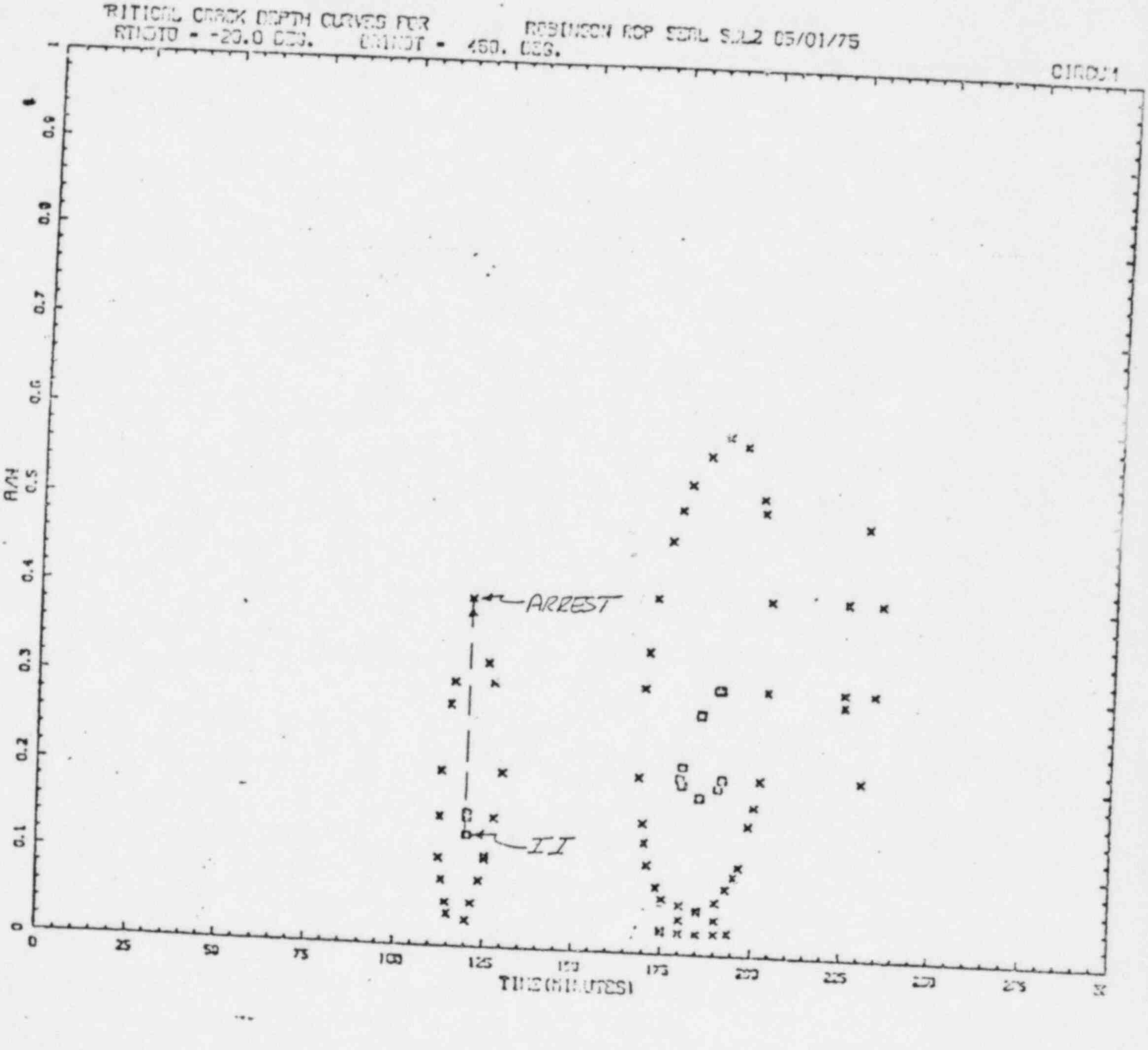


FIGURE 0-15

~~FIG. 15~~

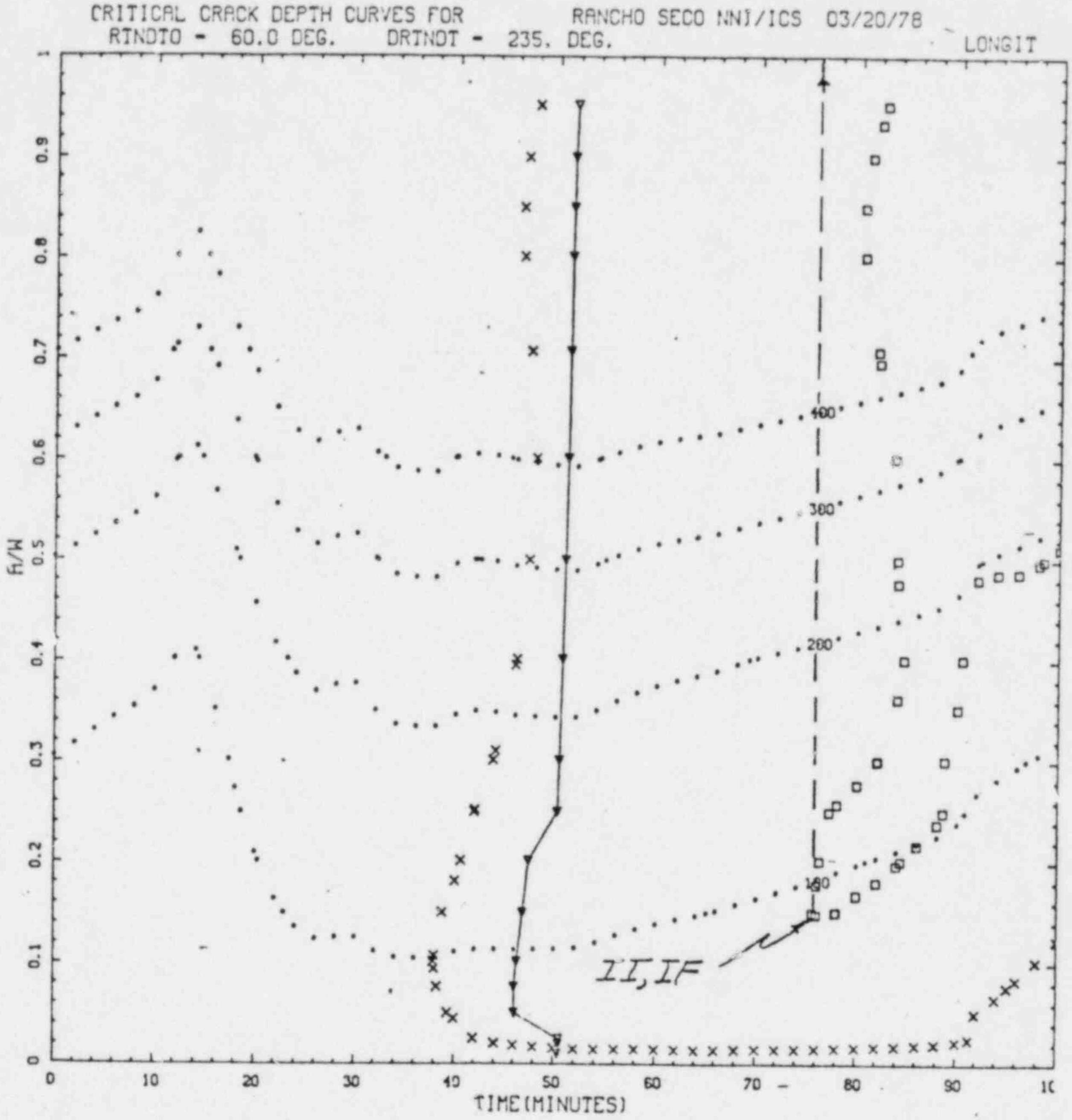


FIGURE 0-16

~~FIG. 16~~

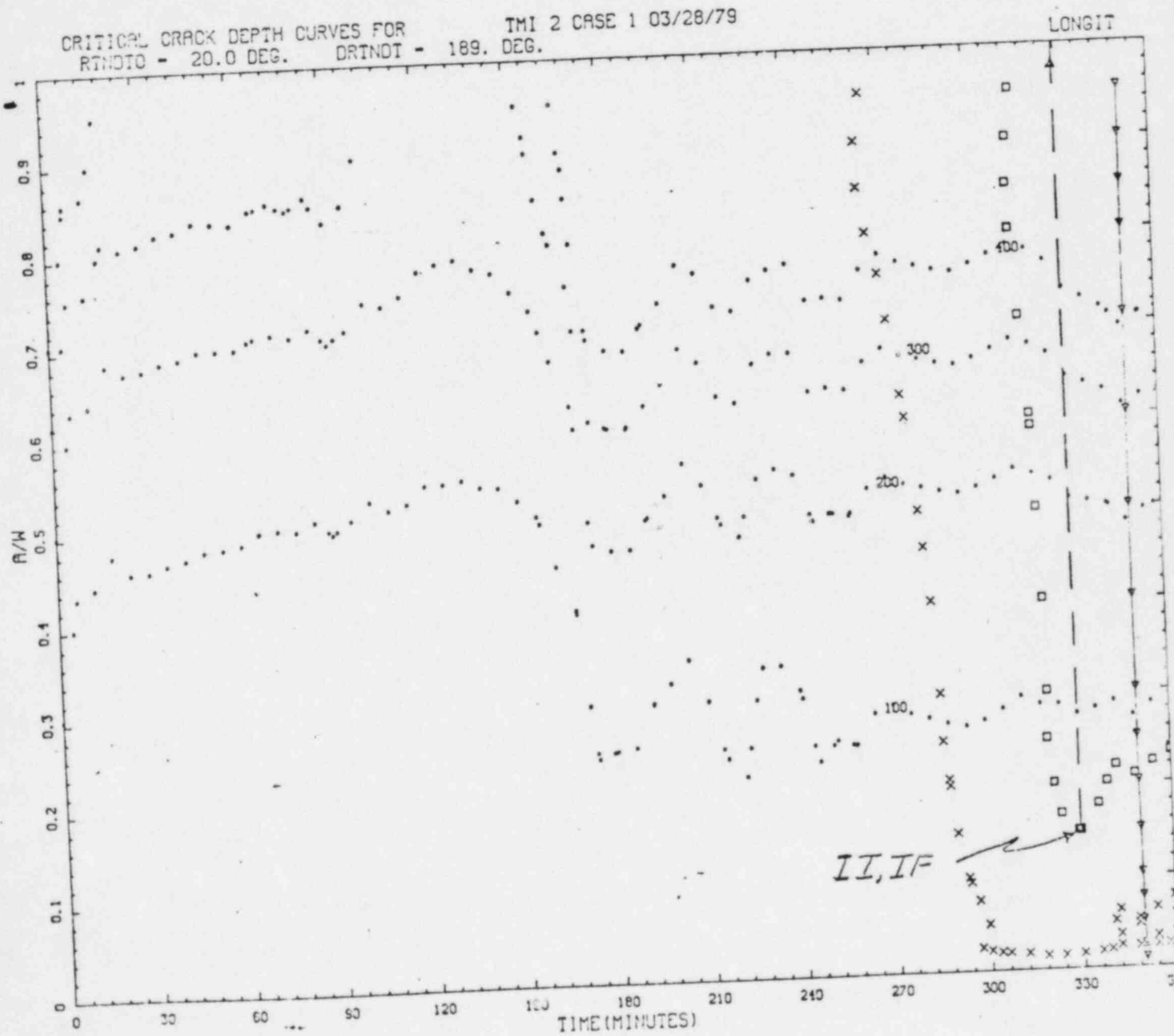


FIGURE 0-17

FIG. 17

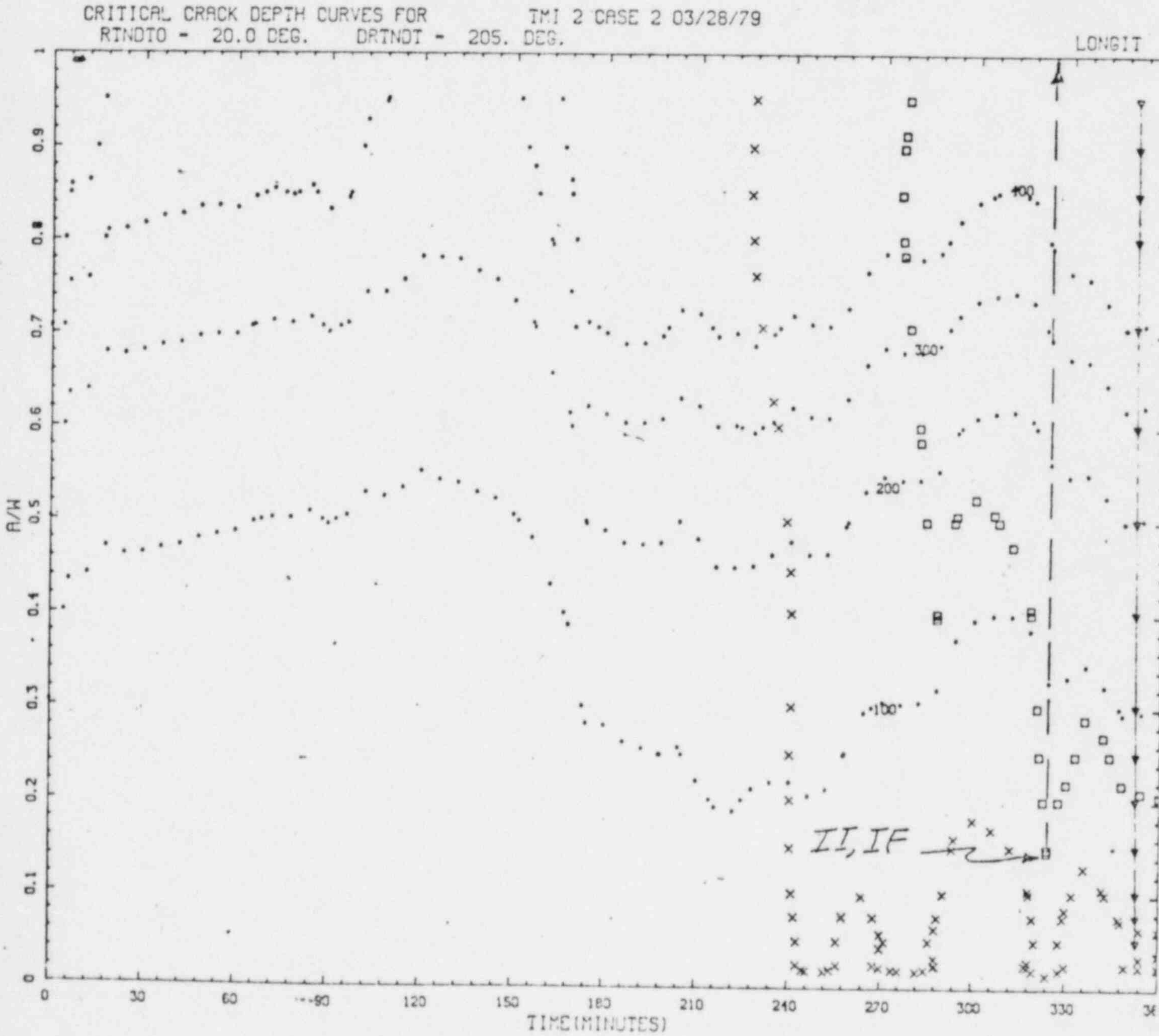
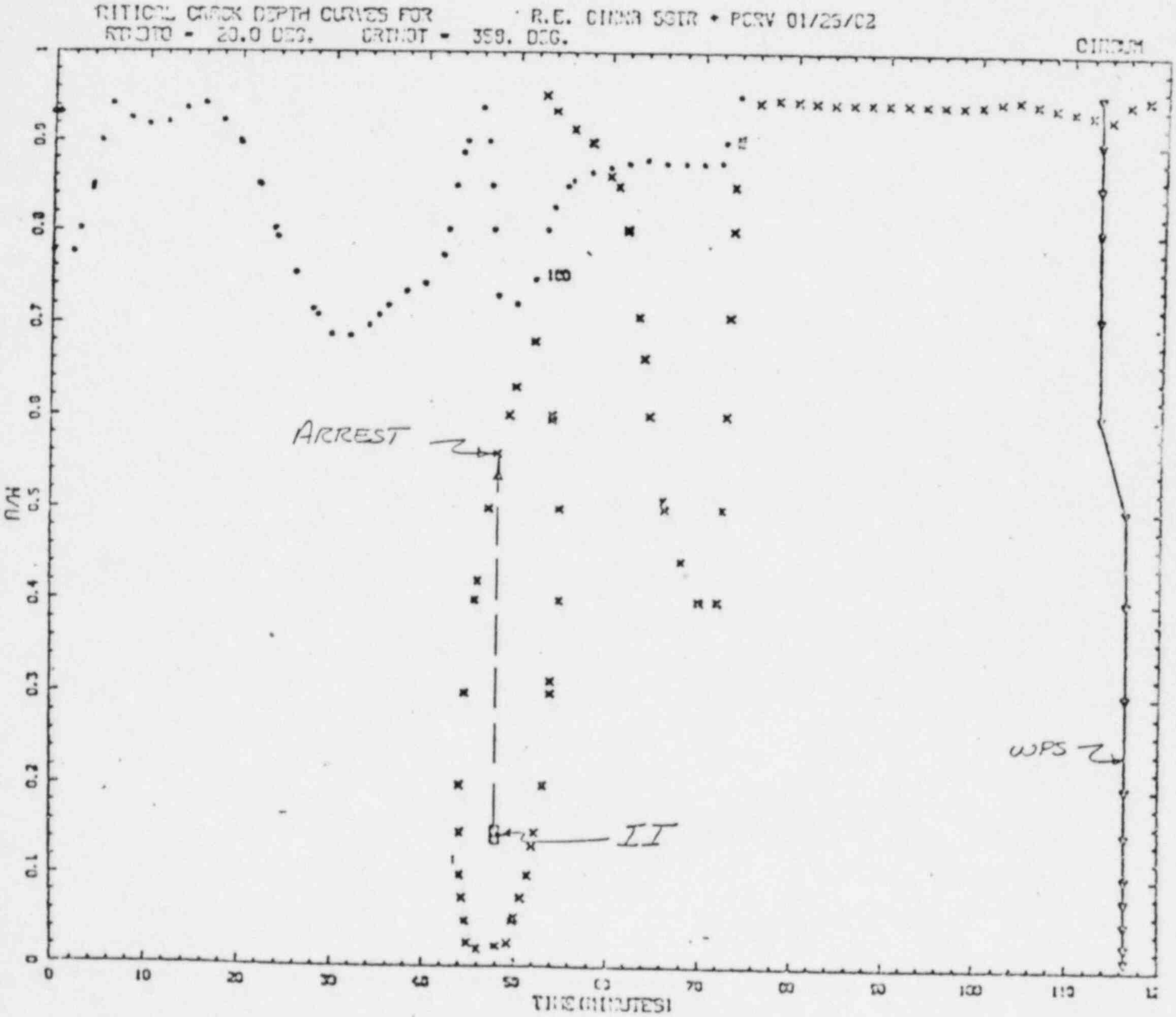


FIGURE 0-18

FIG. 18



0-21 + 0-22

APPENDIX P

CALCULATED RT_{NDT} VALUES FOR PLANTS

P.1 RT_{NDT} Screening Values for All Plants

Table P.1 contains the results of the calculations described in Appendix E for 40 operating PWR plants comprising all of those having significant radiation damage plus all others for which information was readily available. As of June 28, 1982, there were 7 recently licensed PWRs omitted.

In the column headed "Recommended RT_{NDT} Value for Screening" separate values are given for circumferential and axial welds, because the stress intensity factors produced by certain transients are different for the two cases. For many transients for which pressure is high, the critical value of RT_{NDT} is at least 30 degrees higher for circumferential cracks. Plants are listed in descending order of RT_{NDT}, taking that difference into consideration. For plants where the plate or forging governs, its RT_{NDT} value is listed in both columns. Repeating from Appendix E, the recommended RT_{NDT} is the sum of the mean initial RT_{NDT}, the mean ΔRT_{NDT} at the inner vessel wall and the "2-sigma" term.

The column titled "RT_{NDT} after 3 additional FPY" was calculated assuming that fluence per effective full power years (EFPY) remained the same as the average during the service life up to December 31, 1981, i.e., no consideration was given to changes in fuel management practices at some plants, because the specifics were not readily available.

The increase in RT_{NDT} per EFPY gets progressively smaller with the years. The median value is 6.5, and the range is from 2 to 17 degrees F per year for the 3 year period shown in the table.

The sources of information from the various plants are as follows. The EFPY are calculated from data submitted monthly to the NRC for total megawatt hours thermal. This value is divided by the rated thermal power to get effective full power hours. For fluence, copper, and nickel content, the 8 plants that had been identified in August 1981 as having potential for sensitivity to a PTS event had submitted reports containing the results of a recent review of all available data. These 8 plants can be identified in the Table by their values for "Licensee's RT_{NDT}." Most of the other plants had submitted detailed information on their vessel beltline materials and fluence at the critical locations in response to an inquiry from the NRC in May 1977. Finally, there were surveillance reports for a number of plants, which contained updated calculations of fluence at the vessel wall.

P.2 <u>Comparison of NRC and Licensee's Values</u>

For the 8 plants, Table P.1 shows licensee's values of RT_{NDT}. For the three CE plants, Table P.1 also shows the values calculated by CE in Appendices to CEN-189.[1] (Ref. P.1). These CE values range from 28 to 39 degrees F above the licensees' values, largely because of differences in the estimates of initial RT_{NDT}. For the CE plants, the NRC value of RT_{NDT} falls between the licensee's value and the CE value in one case, agrees with the CE values in one case, and falls 18 degrees F above the CE value in the third case. For the three Westinghouse plants, the NRC value of RT_{NDT} is 17 degrees lower for Robinson 2, and 12 degrees lower for San Onofre. For Turkey Point 4, the NRC value is 58 degrees higher, because the licensee used a surveillance value that happens to fall well below the Guthrie mean trend curve. For the B&W plants, the NRC value of RT_{NDT} was 31 degrees higher for Oconee 1 because the NRC treatment of ΔRT_{NDT} gives a higher value than the trend curve from Regulatory Guide 1.99, which B&W used. For Three Mile Island 1, the NRC value of RT_{NDT} was 59 degrees higher than the licensee's value because: (a) they used an initial value of RT_{NDT} of -14°F whereas the NRC used a mean value of 0°F and 2 sigma of 34 degrees F, and (b) they used Regulatory Guide 1.99 as described above for Oconee 1. Actually, B&W did not give the values quoted in the Table. Those values were calculated by the NRC, using copper and fluence values from proprietary references given by B&W. These differences will have to be resolved for those plants that fail the screening criterion.

Table P.1 RT_{NDT} Values for All Plants[1] Calculated Per the Recommendations of the Working Group on RT_{NDT}[2] for the Vessel Inside Surface

Plant NSSS/Vessel Fabricators	EFPY as of 12/31/81	Fluence n/cm2 x1018	Copper %	Nickel %	Mean Initial RT_{NDT}, °F	Mean WRT_{NDT} °F	$2\sqrt{\sigma_o^2+\sigma_\Delta^2}$ (5)	RT_{NDT} Value as of Dec 31, 1981(6) Circum.	Axial	Licensee's RT_{NDT} °F	RT_{NDT} After 3 Additional EFPY (9) Circum.	Axial
Robinson 2	7.10	(14.1)(3)(12)	(0.35)	(1.20)	(-56)	(295)(4)	34 (4)	281		290	292 (10)(13)	
W/CE		14.8 (3)(12)	0.27	0.20	-56	151	59		154	220		162 (13)
Fort Calhoun	5.07	(7.04	(0.35)	0.99	(-56)	(264)(4)	34(4)	242		(7)	267	
CE/CE		5.1 (8)	0.35	0.99	-56	248 (4)	34 (4)		226	209 (239)		250
Turkey Point 4	5.67	9.1 (11)	(0.32)	(0.57)	(0)	(200)	59	259		211	282 (13)	
W/B&W	No Axial Welds											
Turkey Point 3	5.67	(9.1) ~~(9.1)~~ (11)	(0.32)	(0.57)	(0)	(200)	59	259			282(13)	
W/B&W	No Axial Welds											
Maine Yankee	5.90	(5.02)	(0.36)	(0.99)	(-56)	(248) (4)	34 (4)	226		(7)	246	
CE/CE		4.14	0.36	0.99	-56	238 (4)	34 (4)		216	170 (198)		236
Calvert Cliffs 1	4.65	(6.84)	(0.30)	(0.18)	(-56)	(135)	59	138		(7)	158	
CE/CE		6.84	0.30	0.99	-56	212	59		215	205 (244)		246
Indian Point 3	2.98	(1.67)	(0.24)	(0.52)	(+74)	(90)	48	212			231	
W/CE	Plate Governs		0.24	0.52	+74	90	48		212			231
Yankee Rowe	14.56	(11.35)	(0.20)	(0.63)	(+30)	(133)	48	211			217	
W/B&W	Plate Governs		0.20	0.63	+30	133	48		211			217
Rancho Seco	3.54	(2.33)	(0.31)	(0.59)	(0)	(135)	59	194			218	
B&W/B&W		2.05	0.35	0.59	0	148	59		207			233
Three Mile Island 1	3.52	(1.87)	(0.31)	(0.68)	(0)	(133)	59	192		(129)	216	
B&W/B&W		(1.87)	0.35	0.60	0	145	59		204	145		230
Oconee 2	4.71	(2.87)	(0.35)	(0.71)	(0)	(172)	59	231			256	
B&W/B&W		No Axial Welds										

See footnote(s), last page of table

Table P-1 (Continued)

Plant NSSS/Vessel Fabricators	EFPY as of 12/31/81	Fluence n/cm2 x1018	Copper %	Nickel %	Mean Initial RT_{NDT}, °F	Mean WRT_{NDT} °F	$2\sqrt{\sigma_0^2+\sigma_\Delta^2}$ (5)	RT_{NDT} Value as of Dec 31, 1981(6) Circum.	Axial	Licensee's RT_{NDT} °F	RT_{NDT} After 3 Additional EFPY (9) Circum.	Axial
Zion 1	4.97	(3.13)	(0.35)	(0.59)	(0)	(166)	59	225			247	
W/B&W		0.99	0.31	0.61	0	108	59		167			182
Point Beach 1	8.07	(10.01)	(0.24)	(0.57)	(0)	(151)	59	210			223	
W/B&W		7.34	0.24	0.57	0	139	59		198			210
Oconee 1	5.04	(2.73)	(0.26)	(0.61)	(0)	(118)	59	177			193	
B&W/B&W		2.32	0.31	0.55	0	132	59		191	160		208
Indian Point 2	440	No Circum Data										
W/CE		2.2	0.34	1.2	-56	211 (4)	34		189			211
Ginna	8.18	(9.49)	(0.25)	(0.56)	(0)	(154)	59	213			227	
W/B&W		No Axial Welds										
Point Beach 2	7.54	(9.35)	(0.25)	(0.59)	(0)	(156)	59	215			230	
W/ B&W, CE		No Axial Welds										
Arkansas ANO-1	4.42	(2.70)	(0.31)	(0.59)	(0)	(140)	59	199			220	
B&W/B&W		1.99	0.31	0.59	0	129	59		188			208
San Onofre	9.04	(33.45)	(0.27)	(0.20)	(-56)	(188)	59	191		203	206	
W/CE		27.12	0.27	0.20	-56	178	59		181			195
Zion 2	4.49	(2.93)	(0.26)	(0.61)	(0)	(119)	59	178			196	
B&W/B&W		0.90	0.35	0.59	0	118	59		177			195
Palisades	4.12	(4.78)	(0.25)	(1.2)	(-56)	(174)	59	177			205	
CE/CE		4.78	0.25	1.2	-56	174	59		177			205
Crystal River 3	2.48	(1.44)	(0.35)	(0.59)	(0)	(134)	59	193			225	
B&W/B&W		1.36	0.31	0.61	0	118	59		177			205

Table P-1 (Continued)

Plant NSSS/Vessel Fabricators	EFPY as of 12/31/81	Fluence n/cm2 $\times 10^{18}$	Copper %	Nickel %	Mean Initial RT_{NDT}, °F	Mean WRT_{NDT} °F	$2\sqrt{\sigma_o^2+\sigma_\Delta^2}$ (5)	RT_{NDT} Value as of Dec 31, 1981(6) Circum.	Axial	Licensee's RT_{NDT} °F	RT_{NDT} After 3 Additional EFPY (9) Circum.	Axial
Surry 1	4.88	(7.61)	(0.25)	(0.51)	(0)	(141)	59	200			220	
W/B&W		1.66	0.21	0.59	0	81	59		140			152
Cook 1	4.56	(2.87)	(0.40)	(0.82)	(-56)	(222) (4)	34	200			223	
W/CE		1.55	0.13	0.99	-56	58	59		61			70
North Anna 1	2.41	(4.42)	(0.14)	(0.80)	(+38)	(76)	48	162			181	
W/RD		No Axial Welds		Forging	Governs	48		162				
Beaver Valley	1.87	(3.16)	(0.37)	(0.62)	(-56)	(179)	59	182			235	
W/CE		0.47	0.36	0.62	-56	104	59		107			138
North Anna 2	0.77	(1.38)	(0.13)	(0.83)	(+56)	(52)	48	152			184	
W/RD		No Axial Welds		Forging	Governs	48		152				
Salem 1	2.26	(1.49)	(0.24)	(0.51)	(+15)	(87)	48	150			172	
W/CE			0.24	0.51	Plate Governs	87	48		150			172
Oconee 3	4.78	(2.92)	(0.24)	(0.63)	(0)	(112)	59	(171)			186	
B&W/B&W		No Axial Welds										
Surry 2	4.83	(7.54)	(0.19)	(0.56)	(0)	(108)	59	167			192	
W/B&W, RD		1.64	0.21	0.59	0	81	59		140			151
St. Lucie	3.52	(2.22)	(0.31)	(0.11)	(-56)	(98)	59	{101}			119	
CE/CE		2.22	0.30	0.64	-56	132	59		135			159
Calvert Cliffs 2	3.63	(5.34)	(0.30)	(0.18)	(-56)	(127)	59	{130}			149	
CE/CE			0.30	0.18	-56	127	59		130			149

Table P-1 (Continued)

(5) σ_{ω} (17°F) and σ_{Δ} (24°F) are the standard deviations of the intial RT_{NDT} and WRT_{NDT}, respectively, from a generic data base. If plate or forging governed, actual initial RTNDT was available and $s_o = 0$

(6) The sum of the Mean Initial RT_{NDT}, the mean ΔRT_{NDT} and $2\sqrt{\sigma_o^2+\sigma_\Delta^2}$, as of Dec. 31, 1981.

(7) Initial RT_{NDT} assumed by licensee to be -50°F and by CE to be -20°F. Values in parentheses are by CE.

(8) Fluence reduced to 0.73 x peak per Telex from Omaha PPD, Sept. 1, 1982.

(9) As determined by average fluence rate to date. Implementation of low leakage fuel regimes would result in lower values of RT_{NDT}.

(10) The increase in RT_{NDT} gets progressively smaller with the years but a rough number is Col. 15 minus Col. 11 divided by 3.

(11) Fluence reduced from 11.16 n/cm^2 per letter from FPL Aug. 31, 1982, in TP 4. TP 3 tentatively assumed to be the same as TP 4.

(12) Fluence increased per letter from CP&L Co., Sept. 8, 1982.

(13) Low Leakage Cores considered.

Table P-1 (Continued)

Plant NSSS/Vessel Fabricators	EFPY as of 12/31/81	Fluence n/cm2 x1018	Copper %	Nickel %	Mean Initial RT_{NDT}, °F	Mean WRT_{NDT} °F	$2\sqrt{\sigma_o^2+\sigma_\Delta^2}$ (5)	RT_{NDT} Value as of Dec 31, 1981(6) Circum.	Axial	Licensee's RT_{NDT} °F	RT_{NDT} After 3 Additional EFPY (9) Circum.	Axial
Trojan	3.00	(2.07)	(0.16)	(0.62)	(+10)	(65)	48	123			137	
W CBI					Plate Governs		48		123			137
Davis Besse 1	1.68	(1.11)	(0.24)	(0.61)	(0)	(85)	59	144			171	
B&W/B&W		No Axial Welds										
Haddam Neck	10.92	(14.30)	(0.22)	(0.10)	(-56)	(111)	59	{114}			122	
W/CE		11.90	0.22	0.10	-56	106	59		109			116
Kewaunee	5.87	(7.86)	(0.20)	(0.77)	(-56)	(129)	59	132			147	
W/CE		No Axial Welds										
Farley 1	2.19	(3.70)	(0.24)	(0.60)	(-56)	(117)	59	120			128	
W/CE		0.83	0.27	0.60	-56	89	59		92			112
Millstone 2	3.91	(2.19)	(0.37)	(0.06)	(-56)	(114)	59	{117}			136	
CE/CE		No Data for Axial Welds										
Prairie Island 1	5.62	(7.53)	(0.19)	(0.13)	(-56)	(81)	59	84			94	
W/SFAC		No Axial Welds										
Prairie Island 2	5.90	(7.90)	(0.14)	(0.17)	(-56)	(60)	59	63			70	
W/SFAC		No Axial Welds										

(1) Arranged in descending order of the Recommended RT_{NDT}, considering circumferential to be 30 degress less severe than axial orientations.

(2) Memorandum, M. Vagins to S. Hanauer, June, 1982.

(3) Values shown in parentheses on top line are for circumferential welds, bottom line is for axial welds. When plate governs--both lines.

(4) Determined by Reg. Guide 1.99, Rev. 1, Upper Limit Line, $\sigma\Delta = \emptyset$.