(Approval No. 3150-0011)

NUREG-0744
Vol. 1, Rev. 1

Resolution of the Task A-11 Reactor Vessel Materials Toughness Safety Issue

Part I - Main Report, Part II - Staff Responses to Public Comments, and Appendices A and B

U.S. Nuclear Regulatory Commission

Office of Nuclear Reactor Regulation

R. Johnson

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NUREG-0744
Vol. 1, Rev. 1

Resolution of the Task A-11 Reactor Vessel Materials Toughness Safety Issue

Part I - Main Report, Part II - Staff Responses to Public Comments, and Appendices A and B

Manuscript Completed: July 1982
Date Published: October 1982

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ABSTRACT

This report provides the NRC position with respect to the reactor pressure vessel safety analysis required according to the rules given in the Code of Federal Regulations, Title 10 (10 CFR). An analysis is required whenever neutron irradiation reduces the Charpy V-notch upper shelf energy level in the vessel steel to 50 ft-lb or less. Task A-11 was needed because the available engineering methodology for such an analysis utilized linear elastic fracture mechanics principles, which could not fully account for the plastic deformation or stable crack extension expected at upper shelf temperatures. The Task A-11 goal was to develop an elastic-plastic fracture mechanics methodology, applicable to the beltline region of a pressurized water reactor vessel, which could be used in the required safety analysis. The goal was achieved with the help of a team of recognized experts.

Part I of this volume contains the "For Comment" NUREG-0744, originally published in September 1981 and edited to accommodate comments from the public and the NRC staff. Edited segments are noted by vertical marginal lines.

Part II of this volume contains the staff's responses to, and resolution of, the public comments received. A major change to the "For Comment" version of NUREG-0744 involved deletion of proposed safety margins from the regulatory position. Definition of adequate elastic-plastic safety margins will be addressed by the American Society of Mechanical Engineers Boiler and Pressure Vessel Code (ASME Code) Committee, Section XI, at the request of the NRC. When the ASME Code has identified safe margins for normal, upset, and accident conditions, the NRC will review the results and, if the staff finds them acceptable, adopt them by reference. Further discussion can be found in Chapter 6.

This report completed the staff resolution of the Unresolved Safety Issue A-11, "Reactor Vessel Materials Toughness." The information contained in NUREG-0744, Rev. 1, Vol. I (Part I, Part II, Appendices A and B) and Vol. II (Appendices C through K) will be the basis for licensing actions taken by the NRC relative to the toughness requirements in 10 CFR 50, Appendix G.

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VOLUME I

ACKNOWLEDGMENTS

The work reported in this NUREG is the cumulative product of many individuals' efforts. The technical basis for the safety issue resolution was developed by a technical team which is named in Part I. Many of those people continued to make contributions to this effort long after the team was officially dissolved and contractual support terminated. Also, those who submitted comments identified in Part II contributed measurably to the quality of the final products, and their help is gratefully recognized.

INTRODUCTION

The "For Comment" version of NUREG-0744 was issued in September 1981. The technical basis for the resolution of the Task A-11 safety issue was found generally acceptable to the informed public. The response to the request for comments was gratifying. The volume of responses was large, indicating that there is a great interest in the subject. The acceptance of the work even to the point of being complimentary indicated that the solution represents sound engineering thinking. The detail addressed by the respondents proved that the engineering community is well informed on the subject and helped in great measure to make the revised NUREG a polished paper. Based on the comments received from the public and from NRC staff reviewers, the NUREG was revised and edited and is printed here as Part I of NUREG-0744, Rev. 1. The public comments and the NRC staff replies to them form the basis for Part II. Some minor editing was done, but the commentors should be able to find their submittals. The comments were organized by putting the essentially general comments first, followed by those which addressed specific parts of the NUREG, generally in the order of the original document pages at issue. Comments relating to the several appendices to the NUREG were handled in one of several ways. If the issue was editorial (e.g., a typographical error), the NRC Task Manager changed the text. If the issue was a matter of clarification, the Task Manager solicited the help of the author of the specific appendix to frame a reply. If the issue was philosophical, the Task Manager chose to treat the appendix as a Contractor's Report and recognize the right of the author to express his opinions in his own way. None of the last-named class impacted the basic NUREG, which remains the basis for NRC licensing actions.

Needless to say, the completion of Task A-11 does not go very far toward solving all the nuclear reactor pressure vessel problems. The NRC Unresolved Safety Issue A-49, "Pressurized Thermal Shock," deals with a different RPV problem and is considered by many to be one of the major tasks facing the NRC. Elastic-plastic fracture mechanics is still an unfolding technology, and the engineering methods recommended in this report can be expected to undergo significant revision in the years ahead. As discussed in more detail in the NUREG, not all the work undertaken as part of Task A-11 has been finished because safety margins must be quantified by the ASME Code Committee and the data storage and retrieval computer program, "MATSURV," has not yet been certified as correct and complete. What has been accomplished under Task A-11 is believed to be a significant contribution to engineering and reactor safety.

Washington, DC
July 1982

Part I

PART I

NUREG-0744

VOL. 1

FOR COMMENT

RESOLUTION OF THE REACTOR VESSEL MATERIALS TOUGHNESS SAFETY ISSUE

SEPTEMBER 1981

REVISED AND EDITED JULY 1982

NUREG-0744
Vol. 1
For Comment

Resolution of the Task A-11 Reactor Vessel Materials Toughness Safety Issue

Task Action Plan A-11

Main Report and Appendices A and B

Manuscript Completed: April 1981
Date Published: September 1981

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ABSTRACT

The central problem in the unresolved safety issue A-11, "Reactor Vessel Materials Toughness," was to provide guidance in performing analyses required by 10 CFR Part 50, Appendix G, Section V.C. for reactor pressure vessels (RPVs) which fail to meet the toughness requirement during service life as a result of neutron radiation embrittlement. Although the methods of linear-elastic fracture mechanics (LEFM) were adequate for low-temperature RPV problems, their use under operating conditions was questionable because vessel steels, even those which exhibit less than 50 ft-lb of C_v energy, were relatively tough at temperatures where the impact energy reached its upper shelf values. A technical team of recognized experts was organized to assist the NRC staff in addressing the problem. Using the foundation of the J-integral resistance curve and the tearing modulus concept, which had been developed under earlier NRC sponsorship, relationships were obtained which provided approximate solutions to the problem of RPV fracture with assumed beltline region flaws. The first paper of this report is a summary of the problem, the solutions, and the results of verification analyses. The details are provided in a series of appendices in Volumes I and II.

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Volume I

ACKNOWLEDGMENTS

The following individuals participated in the work of the technical team organized to provide a solution to the problem addressed in Task A-11, "Reactor Vessel Material Toughness," and contributed to this evaluation report:

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The following individuals also participated and contributed to the TAP A-11 team effort on a voluntary basis:

J. P. Gudas, Naval Ship Research and Development Center
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Because the technical team did not meet in full committee to resolve perceived errors or disagreements after the NUREG and its appendices were issued, participation must not be construed as implying approval of the entire document.

Resolution of the Task A-11

Reactor Vessel Materials

Toughness Safety Issue

R. E. Johnson, and others

Part I

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FORWARD

NUREG-0744 was issued for public comment in September 1981 to describe the method which has been developed by the NRC staff and contractors and has been found acceptable as a means of complying with 10 CFR 50, Appendix G, Section V.C. The analyses described in NUREG-0744 provide a rational basis for meeting the regulations and do not constitute a substitute for, nor do they countermand, any regulations. Other means of demonstrating that adequate margins against fracture exist in nuclear reactor pressure vessels which fail to meet the toughness requirements of 10 CFR 50 will be accepted if the substitute approach can be shown to have a well-defined theoretical and experimental basis.

1 SUMMARY

Task Action Plan (TAP) A-11, "Reactor Vessel Materials Toughness" (Appendix A to this Report), addressed one of the unresolved safety issues identified by the Nuclear Regulatory Commission (NRC). The fundamental goals of Task A-11 were to provide an improved engineering method to assess the safety margin in nuclear reactor pressure vessels (RPVs), and to develop appropriate new licensing safety criteria for use in the evaluation of normal, transient, or postulated accident conditions. The resulting method would be recommended for the determination of the margin of safety against ductile fracture in RPVs which fail to meet the toughness requirements of the current, relatively simple, criteria. Extensive amounts of prefracture plastic deformation can be expected at high temperatures, even in pressure vessel steels of low toughness. The recommended evaluation method was based on advanced elastic-plastic fracture mechanics concepts. The basis for this improved methodology was published in NUREG-0311, "A Treatment of the Subject of Tearing Instability."[1] Safety margins can be determined by comparing the loads (RPV pressure) for a condition of interest to the calculated failure load where both have been derived from elastic-plastic fracture mechanics concepts. To ensure an adequate margin of safety, the operating (or transient) pressure must remain well below the calculated failure pressure. However, the quantitative relationship may depend on the reactor plant conditions. For example, a much larger margin would be required for normal/upset conditions than for low-probability accident events. The engineering method must account for radiation-induced material degradation.

The need for such an engineering method was dictated by the fact that some materials (primarily weld metals) used in RPVs may have Charpy V-notch (C_v) impact test upper shelf energy (USE) levels of less than 50 ft-lb before the end of their design life.* When RPV steel exhibits a C_v USE level of less than 50 ft-lb, the requirements of Title 10 of the Code of Federal Regulations Part 50 (10 CFR 50)[2] are not being met,** and a safety analysis must be

*Design life is generally considered to be 40 calendar years or 32 years of effective full-power operation (EFPY).

**See footnote on page 3-2 of this report.

performed to ensure continued safe operation of the reactor. Linear-elastic fracture mechanics (LEFM) would be inapplicable because of the large pre-fracture crack tip plastic zones observed in steels with about 50 ft-lb of C_v energy at upper shelf temperatures. As a result, TAP A-11 was designed to provide an acceptable elastic-plastic engineering method. The task focused on the RPV beltline because of the radiation-induced loss of USE in that region. The problem, the proposed solution, verification tests for applicability, and the NRC position are presented in the material that follows.

2 MATERIAL ASPECTS

Steels commonly used in the construction of RPVs exhibit a fracture toughness which varies greatly with temperature. Fracture tests of steel samples as a function of temperature will show relatively high toughness at high temperatures but low toughness at low temperatures. The temperature or temperature range where the transition from high-toughness (ductile) to low-toughness (brittle) behavior occurs is commonly referred to as the ductile-brittle transition temperature. Thus the temperature-dependent fracture toughness has three more or less distinct zones: a lower shelf with low toughness, an intermediate transition region, and an upper shelf with high toughness.

"Size effect" further complicates the problem of assessing fracture toughness. As specimen sizes increase from 0.5 in. to ~12 in., there is an upward shift in the ductile-brittle transition temperature. Although reasons for this effect are complex, it is essentially caused by an increased constraint to local plastic flow in thick sections, or by an increased tendency of the thick sections to maintain plane strain conditions with increasing stress.

Charpy impact test data in the form of specimen fracture energy as a function of temperature reflect the ductile-brittle transition and follow a sigmoidal function. The transition temperature can be identified in several ways, the simplest of which is to report the temperature at an arbitrary C_v energy level (for example, 35 ft-lb). The USE is the energy level of the upper asymptote of the $E_{C-v} = f(T)$ curve.

Neutron radiation from an operating reactor core will embrittle the RPV steel. The embrittlement is shown in two important ways. In one, the transition temperature regime is increased; in the second, the USE is decreased. For the most part, the emphasis in the material that follows will be on the latter because it is more important to TAP A-11.

The embrittling effect of neutron radiation may so change the mechanical properties that the steel in an RPV will fail to meet the toughness requirements of 10 CFR 50.[2] The irregularity could result from either too large a temperature increase in the reference transition temperature (RT_{NDT}), or too large an energy decrease in the C_v USE, or both.

The magnitude of the irradiation-induced changes will depend, among other things, on the chemistry and metallurgical condition of the steel. The effect of copper content can be singled out because it plays a major role in the USE behavior. Copper was introduced by the practice (later abandoned) of copper-coating the consumable electrode weld wire to protect it from rusting and to increase its electrical conductivity. Experiments have shown that the radiation-induced changes in both the transition temperature and the C_v USE increase with copper content, and the most sensitive steels include weld metals with relatively high copper content (in the range of 0.2 to 0.5 w/o). Because some high copper welds exhibited relatively low initial USE levels, the decrease in USE was found to be more significant with respect to violation of regulatory requirements than the corresponding transition temperature increase.

Other important radiation-related comments about RPV steel include the following: Some of the variability in radiation-induced notch ductility changes has been traced to residual element compositional differences, especially the copper and phosphorus contents.[3,4] Special limits on copper and phosphorus contents are included in specifications for nuclear steels from the American Society of Testing Materials (ASTM) and the American Welding Society (AWS). In older steels, welds with high copper and nickel combinations had the highest radiation sensitivity. The experimental evidence suggested that for nickel contents up to about 1 percent, nickel reinforced the detrimental copper effects. Among samples from plates, forgings, and welds, the lowest radiation sensitivities were in forgings.

Regulatory Guide 1.99 (Rev. 1)[5] was prepared to provide conservative measures of the changes in transition temperature and C_v upper shelf* with fluence; copper and phosphorus contents were included parametrically. The guide is updated as significant additional data from surveillance or test reactor

*Regulatory Guide 1.99 contains an operational definition of the upper shelf energy level, as does ASTM Test Method E-185.

programs become available. Conservatism was included by constructing the curves as upper bounds of property changes rather than averages. A different viewpoint will be provided by a document being prepared by the Metal Properties Council (MPC) to present the average transition temperature increase with fluence, including 1σ and 2σ confidence bounds* on the data rather than upper limits. A companion study on upper shelf trends by the MPC is in the very early planning stage.

When the USE decrease dictated by the upper limit curves of the guide is applied to the many steels (plates, forgings, and weld metals) found in domestic operating reactors, it appears that about 20 RPVs will exhibit less than 50 ft-lb before the end of their design life. The standard against which the calculated USE decrease is compared is presented in the following section.

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*Where σ is one standard statistical deviation.

3 REGULATORY ASPECTS

Pressure vessels built to the requirements of the Boiler and Pressure Vessel Code of the American Society of Mechanical Engineers (ASME Code Section III)[6] are expected to withstand pressures more than twice (about three times) their nominal design pressure under normal conditions. That this has been achieved was shown with particular clarity by the series of vessel failure tests run at Oak Ridge National Laboratory (ORNL) over the past 10 years as part of the NRC Heavy Section Steel Technology (HSST) program.[7] In the experiments, intermediate size (39-in.-diameter, 6-in.-thick) vessels built to ASME Code requirements failed only at pressures ranging from about 2.5 to 3 times the design pressures, even though very large flaws were intentionally introduced before testing.

Fracture toughness requirements for RPV steels are given in 10 CFR 50.[2] Most of the details of the requirements can be found in Appendix G to Part 50; the rules for monitoring radiation-induced changes through surveillance programs are given in Appendix H.

The fracture toughness criteria originally adopted were developed by a special Task Group of the Pressure Vessel Research Committee and were recommended for inclusion in the ASME Boiler and Pressure Code in 1971.[8] The criteria were published as the nonmandatory Appendix G to Section III of the Code in the Summer 1972 Addendum. After a thorough review by the Atomic Energy Commission (AEC), the criteria and additional necessary items were incorporated into Appendix G to 10 CFR 50, which became effective in August 1973.

Although the new ASME criteria used LEFM principles exclusively, the difficulty in performing tests to determine valid plane strain fracture toughness led to an approach that employed the two traditional tests: drop weight NDT and Charpy V-notch impact. The goal was to provide a specific safety margin (a factor of 2 on pressure) in the presence of an assumed large flaw (1/4 of the wall thickness was chosen) for all conditions of normal operation, including cold startup and shutdown. A smaller margin (a factor of 1.5) was specified for test conditions.

To perform the necessary calculations, the LEFM fracture toughness (K_{Ic}) as a function of temperature was needed, and it was recognized that data were not available for each and every material in every RPV. This problem was overcome by correlating all available K_{Ic}, K_o, and K_{Ia} test results to the specific nil-ductility transition temperature (NDTT) of the tested material. The lower limit of the data scatter band was used to establish what was called the "reference" toughness curve (symbolized as K_{IR}). Charpy tests also were specified to provide additional assurance that the specific material being evaluated had a normal behavior in the transition temperature region. The somewhat redundant additional tests also provided protection against possible errors in determining the NDT. Also, Charpy specimens were well suited to the experimental determination of the effect of radiation on the fracture mode transition.

All operating reactor licenses require that a surveillance program be maintained in accordance with Appendix H of 10 CFR 50 to monitor irradiation-induced fracture toughness changes. In surveillance programs, specimens are irradiated in operating RPVs, removed according to an established schedule, and tested to provide fracture toughness data. The data are used to determine the conditions under which the vessel can be operated with adequate margins of safety against fracture throughout its service life.

Impact data are used to adjust the K_{IR} curve. The specific index used is the reference temperature for the nil-ductility transition, symbolized as RT_{NDT}; it is defined in the ASME Code. This definition reveals that the reference temperature used to index the K_{IR} curve basically is the drop weight NDT with additional assurance provided by a check at the 50 ft-lb Charpy level. The C_v 50 ft-lb level has been used to measure ΔRT_{NDT}; thus, if the USE level drops to less than 50 ft-lb, ΔRT_{NDT} is infinite, which constitutes a failure to meet the 10 CFR 50 requirements.*

*In Appendix G, Revised (now available for public comment), ΔR_{NDT} is measured at the 30 ft-lb level, but the 50 ft-lb USE requirement is established as a specific attribute.

The RPV material and design requirements of 10 CFR 50 were established to provide ample safety margins for normal and upset conditions during operation. The technical basis for those requirements can be found in Reference 8. The ASME Code recognizes four conditions: normal (Level A), upset (Level B, anticipated transients), emergency (Level C), and faulted (Level D) (a more detailed discussion can be found in Appendix J). The plant conditions covered by Levels A and B are clear, but Level C and D conditions deserve explanation. Quoting from the consequence statements of Section III of the Code (NCA-2421.2):

(a) For Level C (Emergency): "These sets of limits permit large deformations in areas of structural discontinuity. The occurrence of stress to Level C limits may necessitate the removal of the component from service for inspection or repair of damage to the component or support."

(b) For Level D (Faulted): "These sets of limits permit gross general deformations with some consequent loss of dimensional stability and damage requiring repair, which may require removal of the component from service."

Both conditions require shutdown; neither condition implies loss of coolant retention. An emergency condition may require removal from service for repair, but a faulted condition may require permanent removal. Operation after an emergency condition is expected, but it must be assumed that operation after a faulted condition is not possible.

Continuing to extract from Appendix J, the lesser need for the "normal" and "upset" conditions may be illustrated by considering the result of a typical RPV evaluation. A flaw* is assumed to be present, and the pressure-induced stress intensity factor, K_{IP}, is calculated according to

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*Appendix G stipulates: a semi-elliptical flaw normal to the hoop stress, with a depth: $a = T/4$, and a total length at the surface: $l = 3T/2$, where T is the wall (shell) thickness.

$$K_{IP} = C\ (P/2500),\ \text{ksi (in.)}^{\frac{1}{2}}$$

where the factor C depends on wall thickness and P is the internal pressure. Illustrative values are

Wall Thickness, in.	Flaw Depth, in.	K_I at P = 2250 psi, ksi $(\text{in.})^{\frac{1}{2}}$
4	1.0	45
6	1.5	54
8	2.0	63
10	2.5	76

It is required that

$$K_I \leq K_{IR}/\sqrt{10} \simeq K_{IR}/(3.2)$$

Therefore, for a vessel of 8-in. wall thickness (typical of PWRs), K_{IR} should be about 200 ksi $(\text{in.})^{\frac{1}{2}}$, or more, at the operating pressure. That will be so for temperatures of RT_{NDT} + 200°F, or more; that is, for temperatures comfortably below and up to the operating temperature of about 550°F. Therefore, it is believed that the present LEFM procedures are completely adequate and conservative for the evaluation of inservice indications subject to normal and upset conditions.

If the C_v upper shelf remains at or above the 10 CFR 50 requirement of 50 ft-lb, there is no concern about the evaluation of emergency and faulted conditions, because (1) these conditions are not treated in Appendix G and (2) Section XI imposes a relatively low safety factor on these conditions. Therefore, priority must be given to formulating rules applicable to emergency and faulted conditions. Once they are developed, the need for modifications, if any, to the rules for normal and upset conditions should be evident.

When it is determined that the materials toughness requirements of 10 CFR 50 are no longer met, the licensee must complete three tasks; the continued operation of the plant depends on satisfactory completion of all three. First, the RPV beltline region--including all weldments--must undergo a complete nondestructive volumetric examination performed in accordance with the requirements of Section XI of the ASME Code. Second, additional tests must be performed to determine the actual RPV material response to neutron radiation, using archive material, accelerated irradiation, and measured properties such as dynamic fracture toughness. Third, a conservative fracture analysis of the RPV must be performed, including allowance for all uncertainties, to demonstrate the existence of adequate margin during continued operation. If an adequate safety margin cannot be demonstrated by performing the above three-step procedure, continued operation would be contingent upon the successful completion of a thermal anneal to recover sufficient material toughness.

The inspection step may require revision to the inservice examination schedule, with related loss in availability, but it presents no new problems. However, most surveillance specimens are V-notch Charpy bars (a few are small fracture mechanics specimens); therefore, the second step is difficult. The principal problems are (1) interpretation of Charpy data and (2) inadequate testing techniques for the small fracture mechanics specimens. In a later section, a fracture mechanics correlation with Charpy data is proposed based on TAP A-11 work; other efforts are under way to resolve both difficulties.

Several years ago it was recognized that the 10 CFR 50 requirements led directly to a need for advanced fracture mechanics analyses. Experimental evidence showed that violations of the 10 CFR 50 toughness requirements were to be expected because of radiation-induced decreases in Charpy USE to less than 50 ft-lb; thus, the indicated arena for RPV safety margin analysis was marked out as high temperature. Specifically, that would be from as much as 350F° below the typical reactor operating temperature (about 200°F) up to and including the high temperatures which might occur during or as a result of an accident (about 650°F). Within that temperature range, the RPV materials including plates, forgings, and weld metals (even the high copper content welds) which exhibit Charpy V-notch energies on the order of 50 ft-lb are tough enough that the prefracture crack tip plastic zone would exceed the boundary conditions assumed in an LEFM analysis. The resulting dilemma is that, whereas a fracture analysis is required by Section V.C of Appendix G, an LEFM solution would be incorrect and no elastic-plastic fracture mechanics analyses were available with sufficient accuracy and reliablity for nuclear RPV safety margin calculations.

The above considerations resulted in identifying the task of providing a viable high temperature elastic-plastic fracture mechanics analysis for the RPV upper shelf problem as the goal of TAP A-11. The resources of TAP A-11 were organized to provide an engineering methodology, generally acceptable to the NRC, which could be employed to satisfy the third requirement (that is, analysis) of Section V.C of Appendix G to 10 CFR 50.

4 PROBLEM DEFINITION

The issue was to establish an engineering method to assess the safety margin in RPVs which contain steels with 50 ft-lb or less of C_v energy at upper shelf temperatures. The problem area was limited to the beltline region, where neutron flux and radiation sensitivity (especially in some weld metals with relatively high copper content) both were high. The goal was to utilize the current theories of elastic-plastic fracture to develop an engineering method for calculating failure conditions and for evaluating safety margins in operating nuclear reactors.

The low upper shelf mode of failure, which is the focus of TAP A-11, requires some definition. It concerns predicting the burst pressure of a reactor vessel with a beltline flaw of significant depth at temperatures too high for cleavage fracture to occur. One pressure limit relates to the onset of plastic instability in the ligament beneath the flaw. Neutron radiation will increase the pressure required for plastic instability in proportion to the flow stress elevation. However, radiation also reduces the tearing resistance and, if the pressure is to induce full ligament plastic instability, the crack must remain stable as that pressure is applied. When preceded by such a scenario, the final fracture is called the low upper shelf mode of failure.

The A-11 Technical Team decided that the J-integral provides the best basis for an engineering method of elastic-plastic fracture analysis. This means that the crack extension parameter should be either the J-integral itself or a parameter analytically relatable to the J-integral. To ensure an appropriate solution to the problem, the recommended method should consider the actual flaw geometry involved, should include an analysis of the uncracked structure in terms of load and the related strain, and should reduce smoothly to the LEFM solution for linear elastic conditions. From practical considerations, the method of analysis should not require a computerized numerical solution for each individual problem, although it may be based on existing numerical solutions. In addition, the results of calculations should be presentable in a form that has direct physical significance with respect to the safety margin determined for both load and flaw size.

5 PROBLEM SOLUTION

5.1 Theoretical Background

To make efficient use of resources, it was decided to base the solution on available elastic-plastic fracture mechanics concepts. The foundation for a rational theory of elastic-plastic fracture was built from the concept of the Rice J-integral, the Hutchinson-Rice-Rosengren (HRR) analysis for the stress strain fields in the vicinity of a crack,[9] and materials J-resistance curves (known as J-R curves) based on data in the form of: $J = f(\Delta a)$, where Δa is the measured crack extension. A method for applying the J-Integral fracture theory was developed by Prof. Paul C. Paris under NRC sponsorship (Appendix B and References 1, 10, 11, 12, and 13). The theoretical aspects are discussed in Appendix C; their application to the RPV problem is discussed in Appendix H. At the same time, an experimental procedure was needed to obtain material fracture parameters in a form compatible with the theoretical concepts. The single specimen unloading compliance method was adapted and further refined for irradiated specimen testing in hot cells at the U.S. Naval Research Laboratory (NRL).[14] Materials properties are discussed in Appendix D.

The value of J is a measure of the intensity of the stress-strain field (in terms of total work, elastic plus plastic) in the vicinity of a crack. Comparison of J for a structure with the corresponding J for the material of construction leads to a statement of crack equilibrium. The tearing modulus, T, is proportional to the derivative of J with respect to the crack size or, equivalently, to the second derivative of the mechanical energy in the system. Analytical methods of stable crack growth applicable to nuclear pressure vessel steels must be based on the premise that material J-R curves are continuously nonlinear.

It has been shown that ductile fracture depends on the relative value of the J-integral and that the stability of the ensuing crack extension depends on the relative value of T. Thus, a crack would be expected to grow when J_{appl} exceeded a characteristic J_{matl}, say J_{Ic}. The fracture would proceed by stable tearing (under rising load), so long as $T_{appl} < T_{matl}$; it would become unstable when the two values reach equality. The conditions were presented graphically by plotting curves of the related structural and material parameters on a single J-T graph. Details are given in Appendix B.

The distinguishing feature of the methods of analysis presented in this report is a unique application of loading and resistance curves that shows the fracture resistance as a function of the extent of ductile crack growth. As shown in Figure 5.1, up to the point of instability, the applied value of J is less than the critical material J-integral and, at the point of tearing instability,

$$J_{appl} = J_{mat} \tag{5-1}$$

An increase in crack size would cause the applied value of J to increase by more than the related increase in the material J-integral. The failure criterion for tearing instability is Equation (5-1), combined with [15]

$$\frac{dJ}{da} = \frac{dJ_m}{da} \tag{5-2}$$

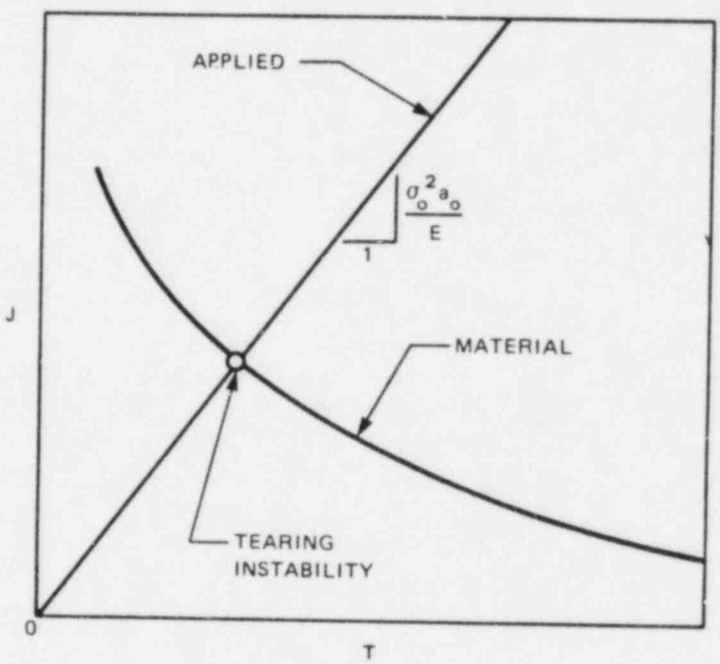

Figure 5.1 Schematic Curves of J=f(T) Illustrating the Material and Structural (Applied) Curves the Intersection Denoting Instability.

The J-R curves shown in Figures 5.2 and 5.3 (taken from Appendix D) show no trend which would suggest a size effect at either the 77 or 302°F (25 or 200°C) test temperature. This result was surprising in view of the likelihood that crack extension occurred beyond the accepted limit of J-control. It suggested that small specimens may be more useful than previously assumed by providing the R-curve trends of larger specimens. Figure 5.4 (also from Appendix D) compares J-R curves for irradiated A 533-B weld metal obtained from two different size CT specimens (0.5 T and 1.6 T).[14] The J-R curves are similar for small crack extensions (Δa less than 1.5 mm). As in Figures 5.2 and 5.3, these data suggest that the small specimen J-R curve is no less conservative than that derived from a larger specimen.

Resistance-curve analysis has been applied by Paris and others[1] to develop a method for estimating the onset of ductile crack instability at limit load under specified boundary conditions. The analysis uses the slope of the resistance curve to determine the nondimensional parameter T, the tearing modulus[15]

$$T = \frac{dJ}{da} \cdot \frac{E}{\sigma_o^2} \tag{5-3}$$

where E is the elastic modulus and σ_o is the flow stress (the average of the tensile yield and ultimate strengths), assumed to govern the magnitude of the fully plastic limit load.

The tearing modulus approach was used to solve two specific pressure vessel fracture problems, namely the simple cylindrical shell with (1) a surface crack and (2) a through-wall crack. The next sections present the following topics: (1) the two fracture problem solutions; (2) a comparison of the approximate (tearing modulus) surface crack solution with a detailed, three-dimensional finite element solution; (3) verification of the solutions by application to (a) the HSST ITVs* and (b) large surface-cracked tensile

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*HSST: heavy-section steel technology research (NRC) program; ITV: intermediate-size test (pressure) vessels.

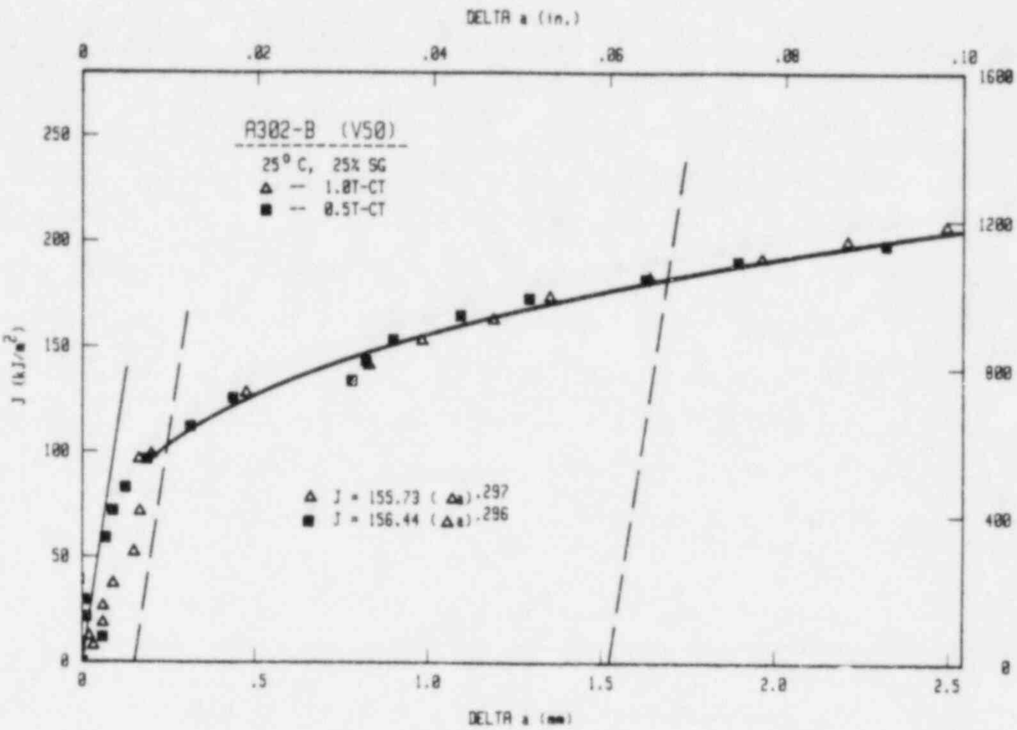


Figure 5.2 J-R Curves From a Low USE A-302B Steel Showing No Size Effect Between 0.5T and 1T CT Specimens at 25°C.

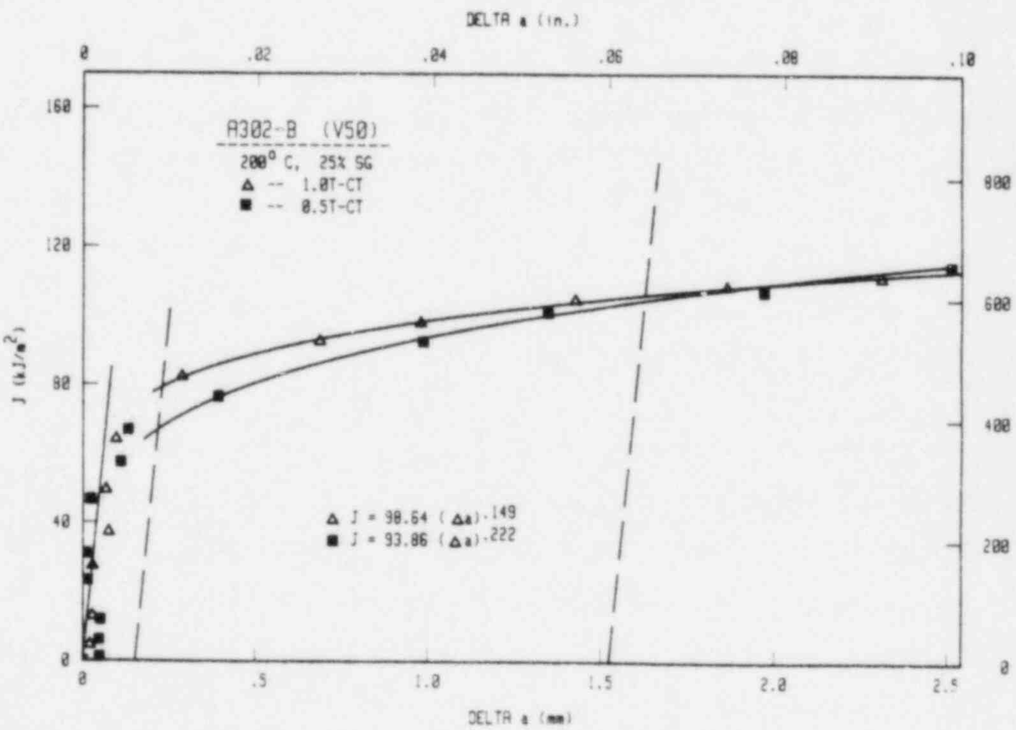


Figure 5.3 The Same as Figure 5.2, for Tests at 200°C, Also Showing No Size Effect.

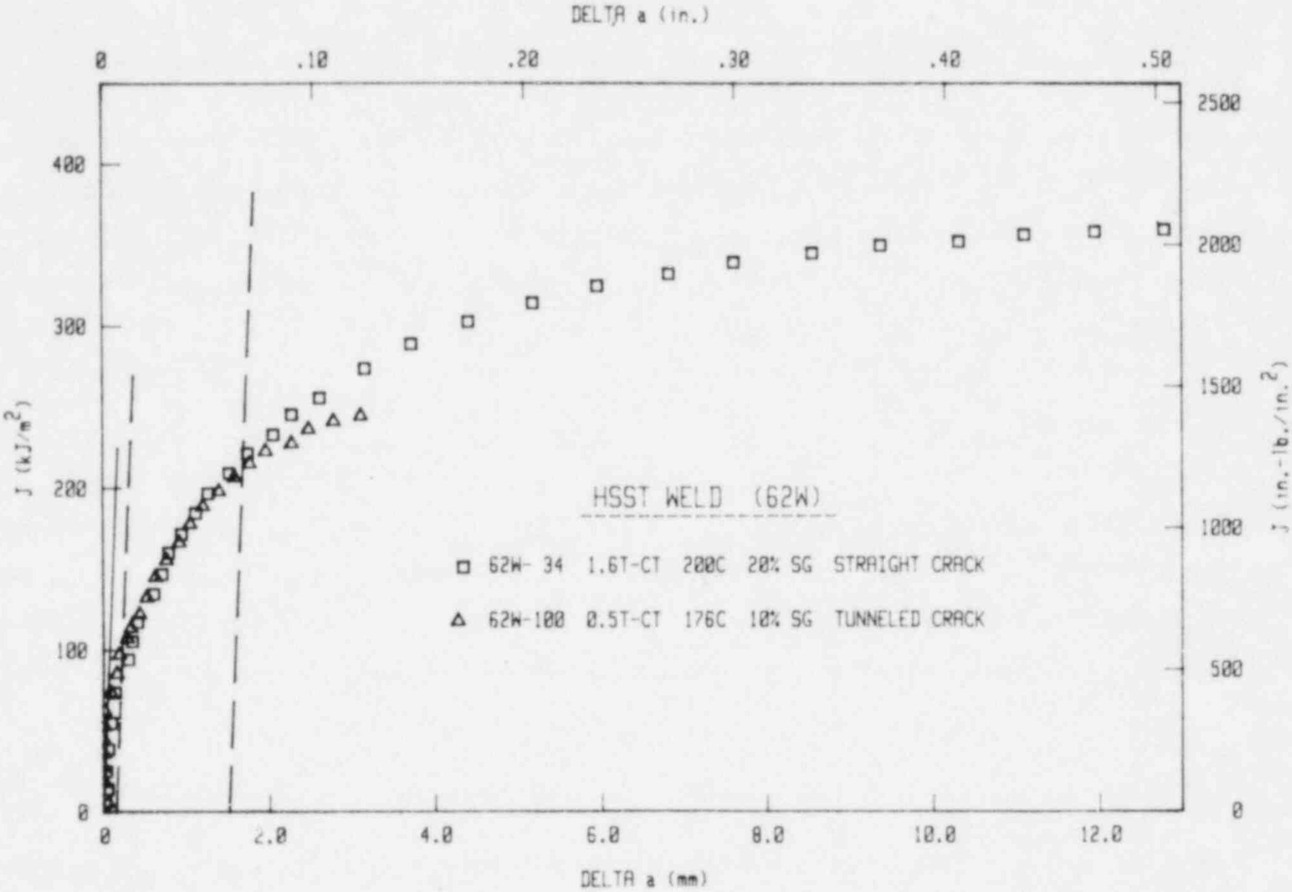


Figure 5.4 J-R Curves from an Irradiated A-533B Submerged Arc Weld Deposit Using 0.5T and 1.6T CT Specimens.

fracture specimens and, in both cases, comparison of the calculated and observed failure conditions; (4) the results of a sensitivity study showing the effect of variability in the underlying mechanical property parameters on the results; and (5) comparison of the tearing modulus approach with other EPFM fracture predictions. Finally, the report addresses the situation where an analysis is necessary, but specific EPFM mechanical property data are unavailable. An alternative is proposed based on a correlation between C_v impact test data and conservatively established values of the J-integral for pressure vessel steels covering a spectrum of metallurgical conditions.

5.2 Elastic-Plastic Analyses

5.2.1 Elastic Approximation

The elastic-plastic analyses of vessel cracks are more easily understood if one first follows through a simple elastic derivation: Consider a wide plate under a uniform tensile stress, σ. Introduce a through-thickness crack of total length 2a normal to the tensile load. The associated stress intensity factor, K, is given by [16]

$$K = \sigma(\pi a)^{\frac{1}{2}} \tag{5-4}$$

In addition to assuming that an LEFM analysis is proper, one also may ignore geometrical differences and apply the equation to a pressure vessel. (In the more detailed analyses, done as part of TAP A-11, shell correction factors were derived and were shown to play an important role.) From LEFM

$$G = K^2/E \tag{5-5}$$

where G is the elastic strain energy release rate for a virtual crack extension; that is, $G = \partial U/\partial a$. In the absence of significant prefracture crack tip plastic deformation, the J-integral reduces to G, so

$$J = G = K^2/E = \pi a\sigma^2/E \tag{5-6}$$

for the wide-plate elastic approximation to the vessel with a through-wall crack. It will be seen later that the plasticity correction factors derived from the TAP A-11 solution also played an important role.

Differentiating Equation 5-6: $dJ/da = \pi\sigma^2/E$, and using Equation 5-3

$$T = \frac{\pi\sigma^2}{E} \cdot \frac{E}{\sigma_o^2} = \pi\left(\frac{\sigma}{\sigma_o}\right)^2 \tag{5-7}$$

Finally, combining Equations 5-6 and 5-7

$$\frac{J}{T} = a\sigma_o^2/E \tag{5-8}$$

The relationship provides a pressure vessel loading curve elastic approximation which, on a graph of $J = f(T)$, will be a straight line except for crack extension. In a large structure with a relatively small initial crack size under slow, stable crack growth (by a ductile tearing mechanism), the straight line will be a good approximation. In combination with a materials curve of $J_{matl} = f(T_{matl})$, as on Figure 5.1, Equation 5-8 would allow a determination of unstable (that is, rapid or catastrophic) fracture conditions.

5.2.2 RPV Analyses

The elastic-plastic fracture analyses for pressure vessels with through-wall or part-through cracks are reported in detail in Appendices B and C of this report. An abbreviated presentation follows.

5.2.2.1 Through-Wall Crack

For a pressure vessel with a through-wall crack of 2a total length, LEFM considerations suggest

$$J = K^2/E \tag{5-9}$$

and $K = \sigma\sqrt{\pi a} \cdot Y(\lambda)$ (5-10)

where Y is a geometrical shell correction factor, a function of $\lambda = a\sqrt{Rt}$.

In turn, R = vessel radius, and t = shell (wall) thickness. So

$$J = \frac{\sigma_o^2 a}{E} \left\{\frac{\pi\sigma^2}{\sigma_o^2}\right\} [Y^2] \tag{5-11}$$

where the convention is adopted that

{ } = the stress bracket (yielding correction factor)

[ ] = the geometry bracket

Operating on Equation 5-11

$$T = \left\{\frac{\pi\sigma^2}{\sigma_o^2}\right\} \cdot [Y^2 + 2\lambda Y \cdot Y'] \tag{5-12}$$

where the prime signifies $\frac{d}{d\lambda}$. Thus

$$\left(\frac{J}{T}\right)_{appl} = \frac{\sigma_o^2 a}{E} \left[\frac{1}{1 + 2\lambda Y'/Y}\right] \tag{5-13}$$

As was the case in the simplified previous example, if $\Delta a = 0$, the loading line is a linear curve. Figures 6, 7, 8, and 9 in Appendix B can be used to obtain values of Y graphically. Also, consideration of plasticity corrections on { } led to the same final result and the conclusion that the same approximate loading line equation applies whether a crack tip plastic zone size correction is used or not. Because the evaluation of the geometry bracket in Equation 5-13 showed that [ ] = 0.5 to 1.0, it was concluded that the simple elastic result of Equation 5-8 was a good first-order approximation.

5.2.2.2 Surface Crack Approximation

For a pressure vessel with a part-through elliptical (surface) crack of depth a and total (surface) length 2c, LEFM analysis suggests

$$K = \frac{\sigma\sqrt{\pi a}}{\phi\left(\frac{a}{c}\right)} \cdot f\left(\frac{a}{c}\right) \cdot g\left(\frac{a}{t}\right) \tag{5-14}$$

where ϕ is the complete elliptic integral of the second kind (a function of the aspect ratio, a/c) and f and g are geometric correction factors for the front surface and back surface, respectively. From Equation 5-9:

$$J = \frac{\sigma_o^2 a}{E} \left\{\frac{\pi\sigma^2}{\sigma_o^2}\right\} \cdot F\left(\frac{a}{c}\right) \cdot G\left(\frac{a}{t}\right) \tag{5-15}$$

where: $F(\frac{a}{c}) = [f(\frac{a}{c})/\phi(\frac{a}{c})]^2$, and $G(\frac{a}{t}) = [g(\frac{a}{t})]^2$

and, according to Equation 5-3

$$T = \left\{\frac{\pi\sigma^2}{\sigma_o^2}\right\} F\left(\frac{a}{c}\right)\left[G\left(\frac{a}{t}\right) + \frac{a}{t}\cdot G'\left(\frac{a}{t}\right)\right] \tag{5-16}$$

In deriving Equation 5-16, F' was ignored because it would result in a small change (zero to slightly negative) for an increase in a relative to c; the result is to make the evaluation of T somewhat conservative. Finally

$$\left(\frac{J}{T}\right)_{appl} \quad \frac{\sigma_o^2 a}{E}\left[\frac{1}{1 + \frac{a}{t}\cdot\frac{G'}{G}}\right] \tag{5-17}$$

The similarity with the equation for the through-wall crack, Equation 5-13, should be obvious on inspection. Taking a well-known approximate relation,

$$G\left(\frac{a}{t}\right) = \sec\frac{\pi a}{2t},$$

the geometry bracket from Equation 5-17 becomes $\left[\frac{1}{1 + \frac{\pi a}{2t}\tan\frac{\pi a}{2t}}\right]$,

and for $0 \leq \frac{a}{t} \leq \frac{1}{2}$; $1 \leq [\quad] \leq 0.57$.

In Appendix B, it is noted that analyses of plasticity effects relative to the influence on the stress bracket, { }, did not alter the conclusion. Thus, for pressure vessels with significant cracks in the beltline (cylindrical) region

$$\left(\frac{J}{T}\right)_{appl} = \frac{\sigma_o^2 a}{E}\,[\Gamma] \tag{5-18}$$

where $0.5 \leq \Gamma \leq 1.0$ for most examples of interest.

5.2.2.3 Comparison to Three-Dimensional Analysis

The General Electric Company conducted a detailed three-dimensional elastic-plastic finite element analysis of a typical pressurized water reactor (PWR) vessel containing a 1/4 T beltline axial flaw.[17] Vessel geometry and modelling

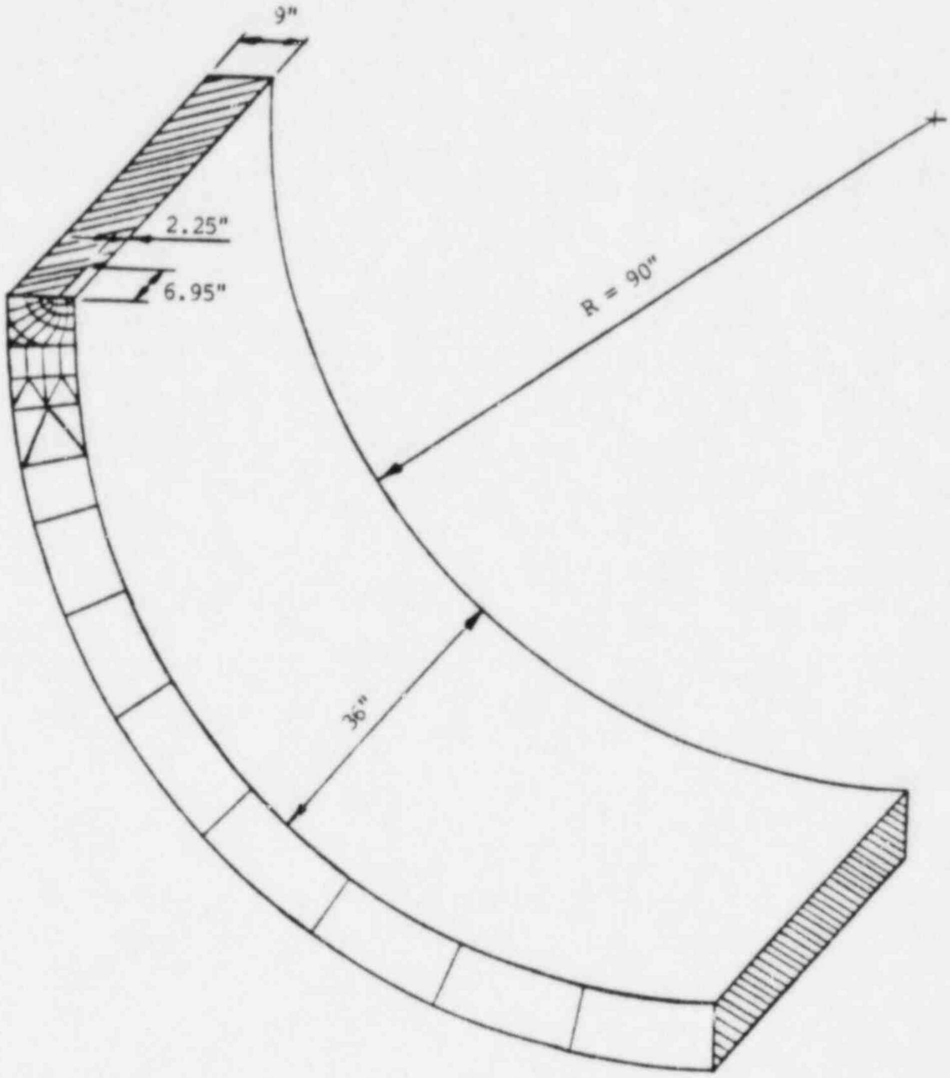


Figure 5.5 Finite Element Model of Vessel with Beltline Axial Flaw

details are illustrated in Figure 5.5. Because this problem is very relevant to TAP A-11, it was decided to compare these results to the approximate, part-through flaw methodology using J/T curves. The estimated J-values from the approximate surface flaw analysis listed in Table 5.1 were plotted in Figure 5.6, along with the three-dimensional, finite element results. The agreement between the two approaches is excellent for the entire range of pressure over which the finite element calculations have been performed. Moreover, the approximate analysis has been carried beyond this point to values of $\sigma/\sigma_o < 1$, and the extension of the analysis looks reasonable also. This agreement further verifies the approximate surface flaw methodology

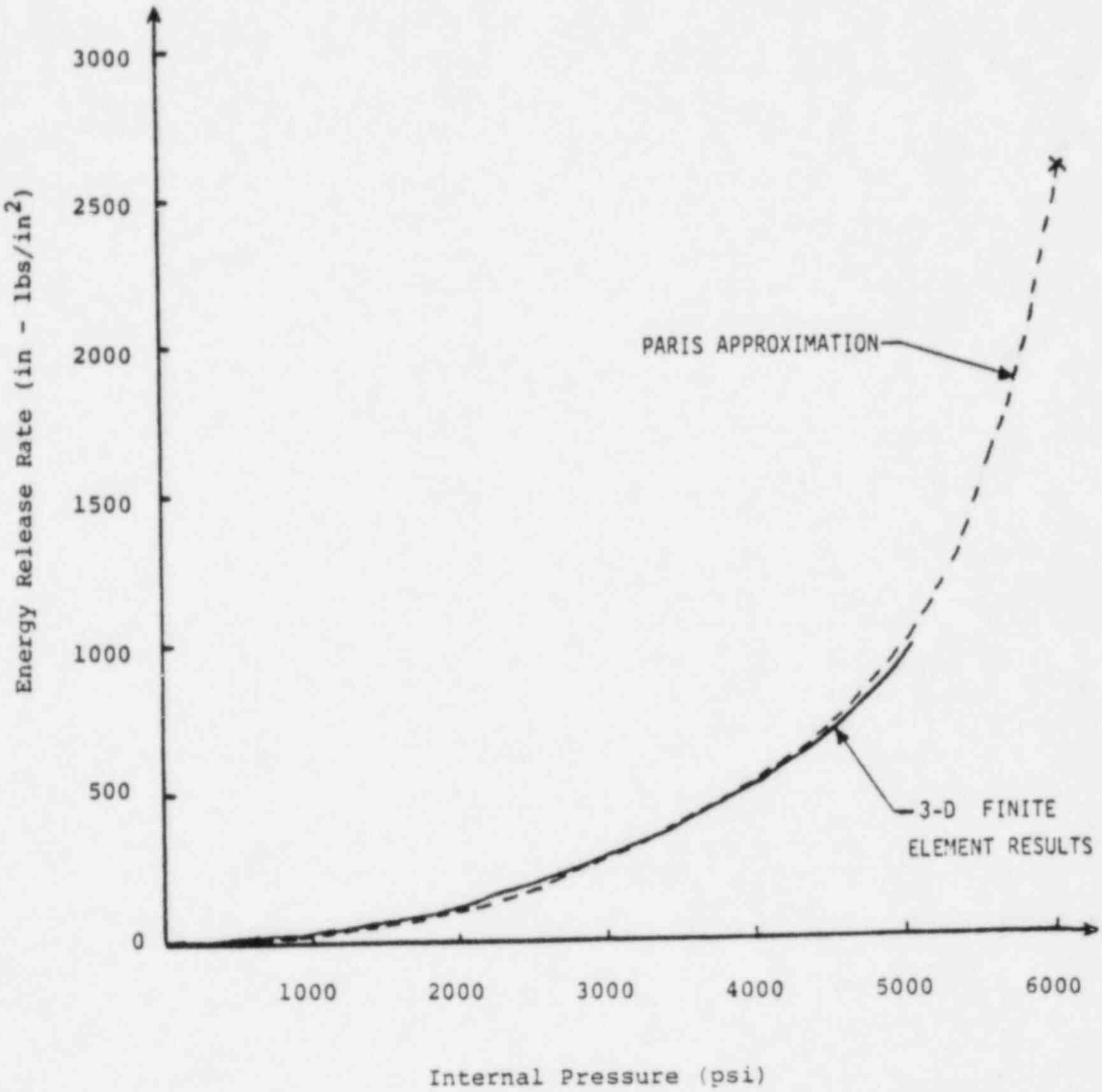
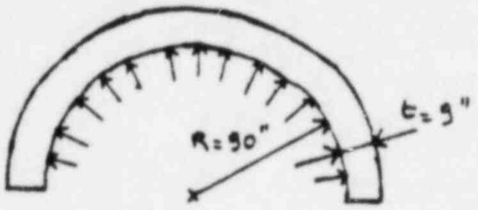


Figure 5.6 Variation of Energy Release Rate with Internal Pressure.

Table 5.1 J-Integral as a function of internal pressure



$\sigma_o = 60{,}000$ psi

$\varepsilon_o = 0.002$

$E = 30 \times 10^6$ psi

$\sigma_\theta = \frac{PR}{t} = 10P$

$$\varepsilon = \varepsilon_e + \varepsilon_p = \frac{\sigma_\theta}{E} + \varepsilon_p\,(\sigma_\theta)$$

$$\frac{\varepsilon}{\varepsilon_o} = \frac{\sigma_\theta}{E\varepsilon_o} + \frac{\varepsilon_p(\sigma_\theta)}{\varepsilon_o}$$

$$\frac{\varepsilon}{\varepsilon_o} = \frac{\sigma_\theta}{\sigma_o} + 1.4\,\left(\frac{\sigma_\theta}{\sigma_o}\right)^{8.6}$$

P	σ_θ	σ_θ/σ_o	$\varepsilon/\varepsilon_o$	$F(\sigma/\sigma_o)$	J
1000	10,000	.17	.17	---	---
2000	20,000	.33	.33	---	---
3000	30,000	.5	.503	.82	261
4000	40,000	.67	.71	1.5	478
5000	50,000	.83	1.11	3.0	956
6000	60,000	1.0	2.4	8.0	2549
7000	70,000	1.17	6.51	24.5	7806

developed under TAP A-11 for application to the RPV beltline region. It also indicates a lack of sensitivity of the stress correction factor $F(\sigma/\sigma_0)$ to the particular stress-strain law, because it was demonstrated for two different stress-strain laws.

5.3 Experimental Verification

5.3.1 HSST ITVs

Further evaluation of the analyses was done by comparing calculated failure conditions to observations. A series of ITVs had been constructed and tested under the HSST program. Very large cracks were introduced artificially and the vessels were pressurized to failure at various test temperatures ranging from the lower transitional to the upper shelf for the specific low alloy steels used. The magnitude and diversity of the ITV program test results provided an opportunity to verify the application of the tearing modulus concepts developed for TAP A-11.

Table 5.2 summarizes the results of the tearing modulus analysis of the surface flawed ITVs. Estimates of J_{appl} and T_{appl} for ITVs V1, V2, V3, V4, and V6 were obtained using an approximate elastic-plastic analytical procedure for part-through flaws in tension presented in detail in Appendix H in Volume II of this report. For vessels V2 and V4, which were tested in the transitional regime, the failure prediction was based on $J = J_{Ic}$ (1200 in.-lb/in.^{2}).

Table 5.2 Comparison of actual and predicted failure conditions for vessels 1 to 4, and 6

Vessel	Test Temp (°F)	Regime	Actual Failure Conditions P (ksi)	ε (%)	Tearing Instability Calculations J_{crit}	ε_{crit} (%)	P_{crit} (ksi)	Plastic Instability Check P (ksi)	Predicted Failure Conditions P (ksi)	ε (%)
V2	32	Transition	27.9	0.19	1200	0.21	27.5	N/A	27.5	0.21
V4	75	Transition	27.0	0.17	1200	0.18	27.2	N/A	27.2	0.18
V1	130	Upper Shelf	27.6	0.92	5000	0.71	27.6	28.0	27.6	0.71
V3	130	Upper Shelf	31.0	1.47	8000	1.06	28.6	30.2	28.6	1.06
V6	190	Upper Shelf	31.9	2.0	8000	1.59	30.4	32.2	30.4	1.59

Entering the curves of Figure 5.7 at the J value of 1200 in.-lb/in.^{2} ($=J_{Ic}$) outer surface hoop strain predictions were obtained of 0.21 and 0.18, in excellent agreement with the actual failure strains. Failure pressure predictions obtained from the pressure/strain curve of Figure 5.8 were also in excellent agreement with the test results. For vessels V1, V3, and V6, which were tested on the upper shelf, values of J_{crit} for instability of 5000, 8000, and 8000 in.-lb/in.^{2}, respectively, were chosen. This was based on the intersection of the best estimate material curves and the J/T applied bands in Figure 5.9. Entering Figure 5.7 at the above J values yielded failure strain predictions of 0.71, 1.06, and 1.59, respectively, for vessels V1, V3, and V6. Once again, failure pressures were obtained from Figure 5.8. The predicted failure conditions all were reasonably conservative underpredictions of the actual experimental failure conditions. Considering some of the approximations used in the analysis, the favorable comparisons were highly encouraging.

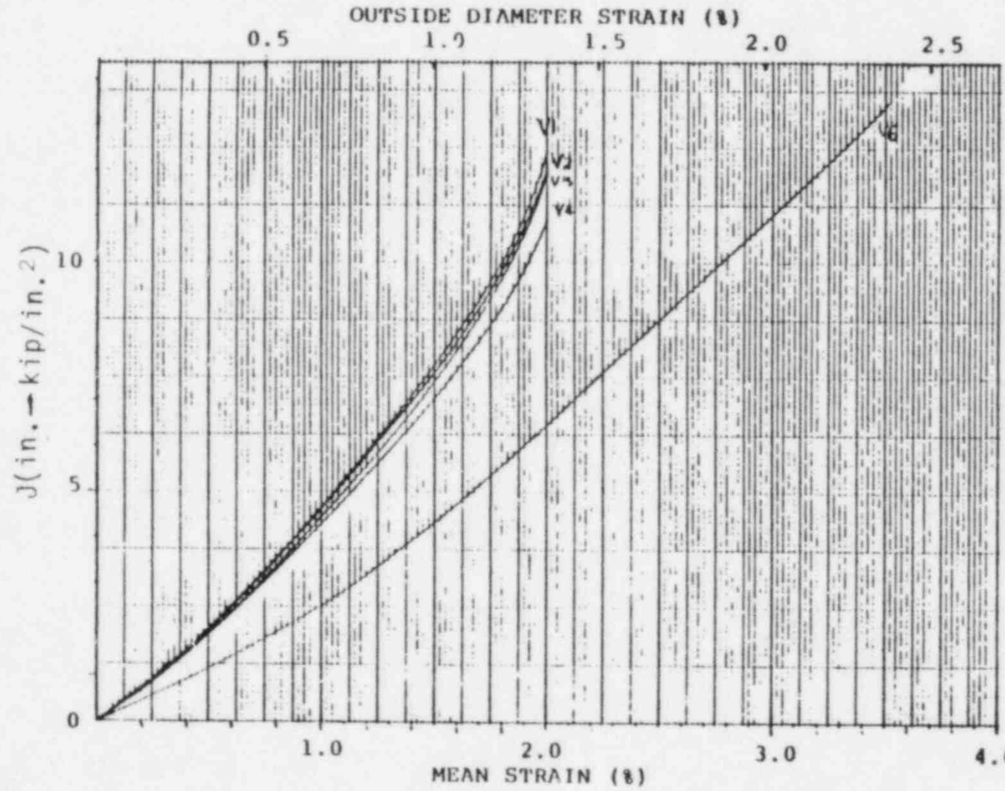


Figure 5.7 J Vs. Mean and Outside Strain for Test Vessels.

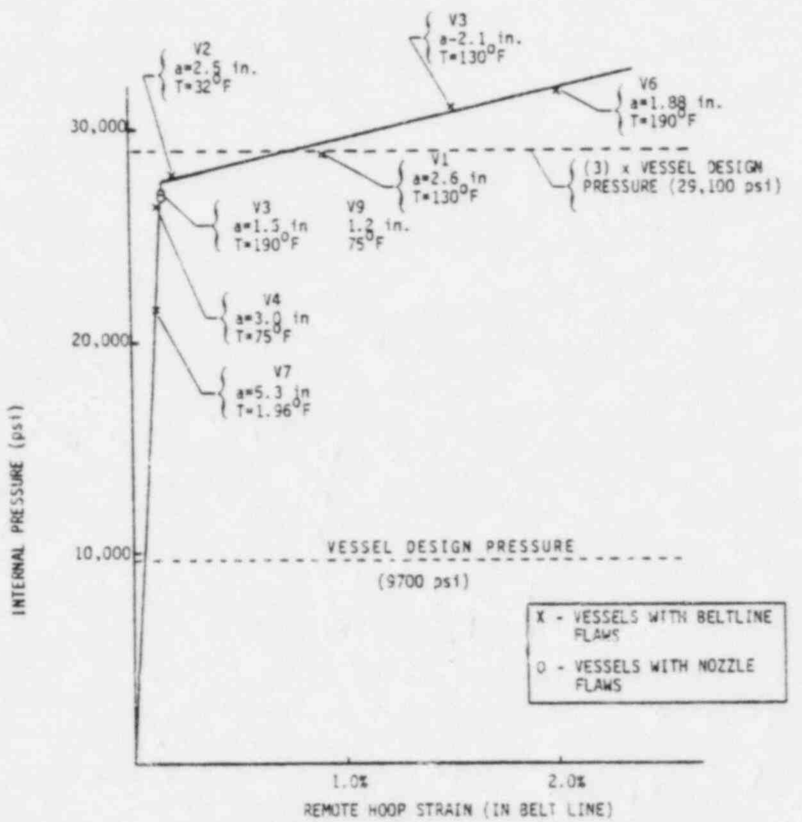


Figure 5.8 HSST Intermediate Pressure Vessels Test Data

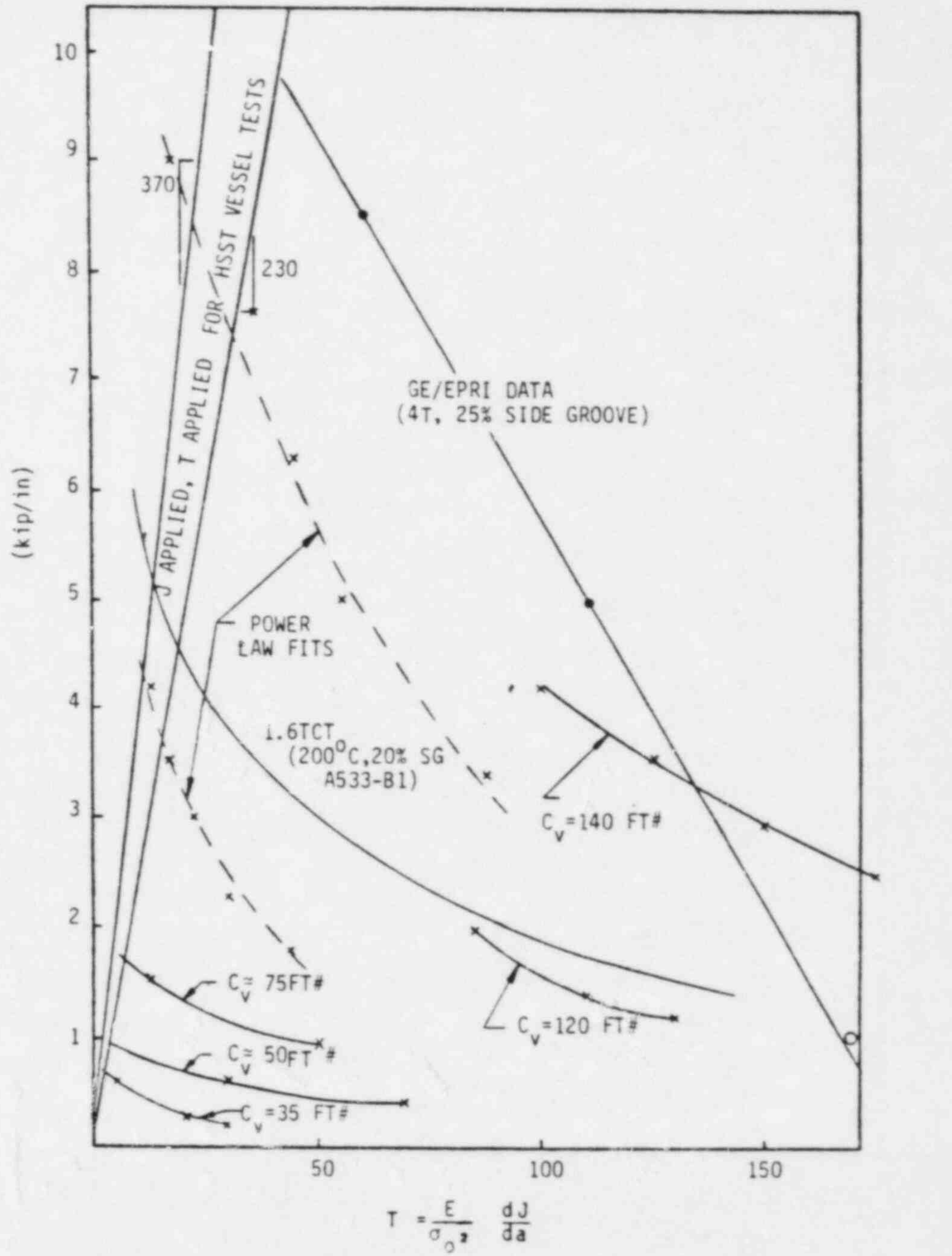


Figure 5.9 J_{mat} Vs. T_{mat} Curves of Figure 6, Appendix H

Vessel V7 contained a 5.3-in. deep flaw, which is almost a through-thickness flaw for the 6-in. wall. During the test, the flaw propagated through the wall and caused a leak, but it did not propagate further. Therefore, in the case of vessel V7, the analysis should predict that a through-wall flaw is stable at the peak test pressure.

Vessel 7 was tested at 190°F (88°C); therefore, the critical value of J was approximately 11 kip/in. Entering this value in Figure 5.10 yielded a failure strain at the outside surface of 0.16%. The measured peak strain was actually 0.12% and no failure occurred; therefore, the analysis correctly predicted the vessel behavior.

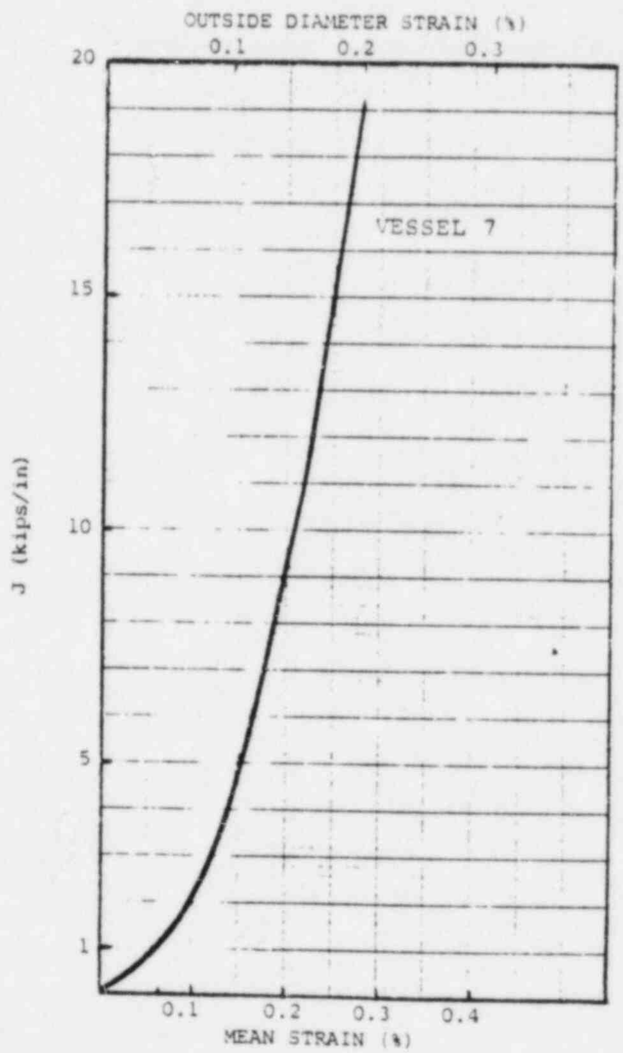


Figure 5.10 J Vs. Mean and Outside Surface Strain for Test Vessel V7

5.3.2 Surface-Cracked Tensile Specimens

The same analysis was used to estimate J_{appl}/T_{appl} curves for a series of large, surface-cracked tensile specimens. However, the analysis neglected the effect of specimen bending induced by the presence of the crack; therefore, it was valid only as a first approximation. Tables 5.3 and 5.4 list geometry parameters and J/T values for the various tests performed. As discussed in Appendix H, the approximate analysis developed by Paris, which was the basis for the values of $F(\sigma/\sigma_o)$ listed in Table 1 in Appendix B, was extended (Table 2, Appendix H) and used to develop curves of J as a function of ε for the tensile specimens tested. The curves were reproduced in Figure 5.11.

Table 5.3 Crack dimensions after sharpening

Specimen	a(in.)	B(in.)	J/T (lb/in.)
1	2.375	6.598	280
2	2.16	6.0	255
3	2.05	8.37	242
4	2.05	8.20	242
5	2.53	8.26	299
6	3.37	9.53	398
14	2.55	7.85	301

Table 5.4 Crack dimensions at ultimate load

Specimen	a(in.)	[illegible]	J/T (lb/in.)
1	--	--	----
2	2.[illegible]	[illegible].91	338
3	2.05	8.37	242
4	3.45	8.02	407
5	2.53	8.26	299
6	4.33	10.25	511
14	2.90	7.85	342

The resulting J_{appl}/T_{appl} calculations were compared to the material properties (J_{matl} and T_{matl}) to predict the failure stresses and strains for the specimens. The predicted fracture strains were conservative estimates of the ultimate strains, except for test 6. This test was performed at 100°F, which is about the limit between transitional and upper shelf regimes. To account for the plane stress conditions, Paris suggested* that the J_{matl}/T_{matl} curves should be modified by doubling the J values. The modified curves are shown on Figure 5.12, with the J_{appl}/T_{appl} curve. The critical value of J is somewhere between 8 and 19 kips/in. Taking J_c as 18 kips/in. yielded new predicted strains at the ultimate; these are listed in Table 5.5 and were in better agreement, although higher than, the experimental ultimate strains.

Table 5.5 Comparison of predicted and actual strains at failure

Specimen	Strain (%) at ultimate	Strain (%) at fracture	Test Temp, °F	Predicted Strain (%) J_c=8kip/in.	Predicted Strain (%) J_c=18kip/in.
1	---	8.50	215	2.3	3.6
2	3.83	9.60	220	2.6	4.5
3	0.24	0.24	50	0.28	---
4	4.10	7.90	100	2.0	3.3
5	0.35	0.35	75	0.28	---
6	0.48	0.48	100	1.60	2.7
14	1.81	4.22	200	1.70	2.9

5.4 Analytical Evaluation

5.4.1 Sensitivity Study

As part of TAP A-11, EG&G Idaho performed a sensitivity analysis, verified correction terms, and performed calculations of critical conditions using alternate analytical techniques. The sensitivity analysis and verification of correction terms were associated with the equation

$$J = \sigma_{ys}\ \varepsilon_{ys}\ a\ \{F(\frac{\sigma}{\sigma_{ys}})\}\ [\frac{M^2}{Q}] \qquad (5\text{-}19)$$

*In a private communication with P. Riciardella, as cited in Appendix H to this report.

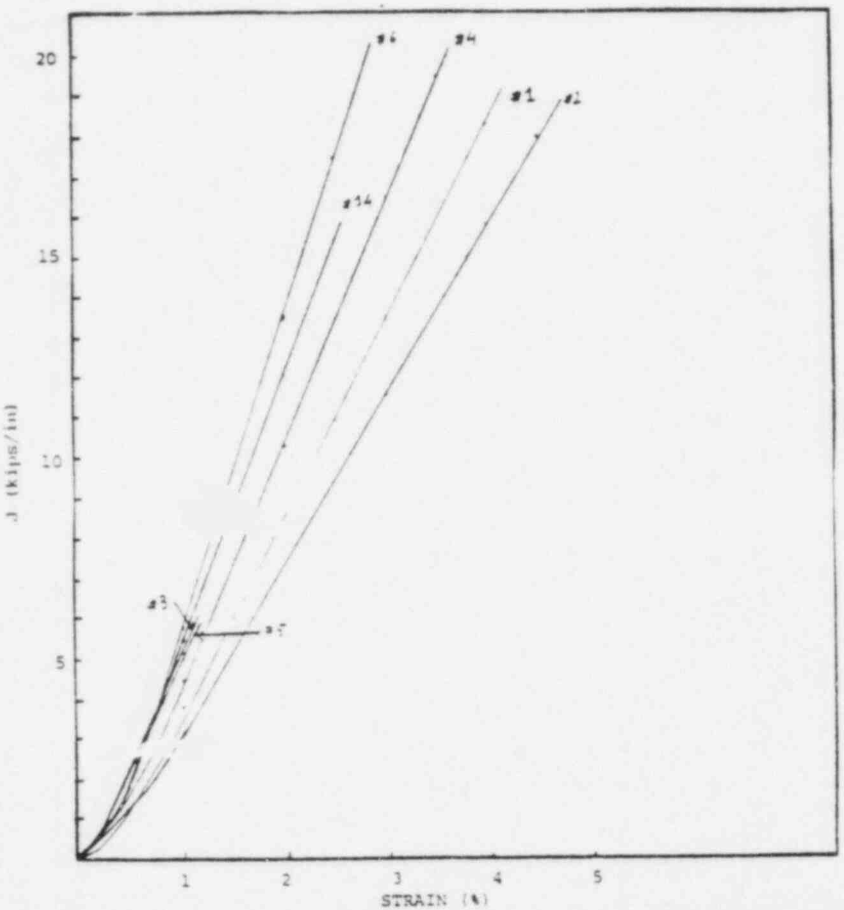


Figure 5.11 $J_{applied}$ Vs. Strain for the Large Tensile Specimens

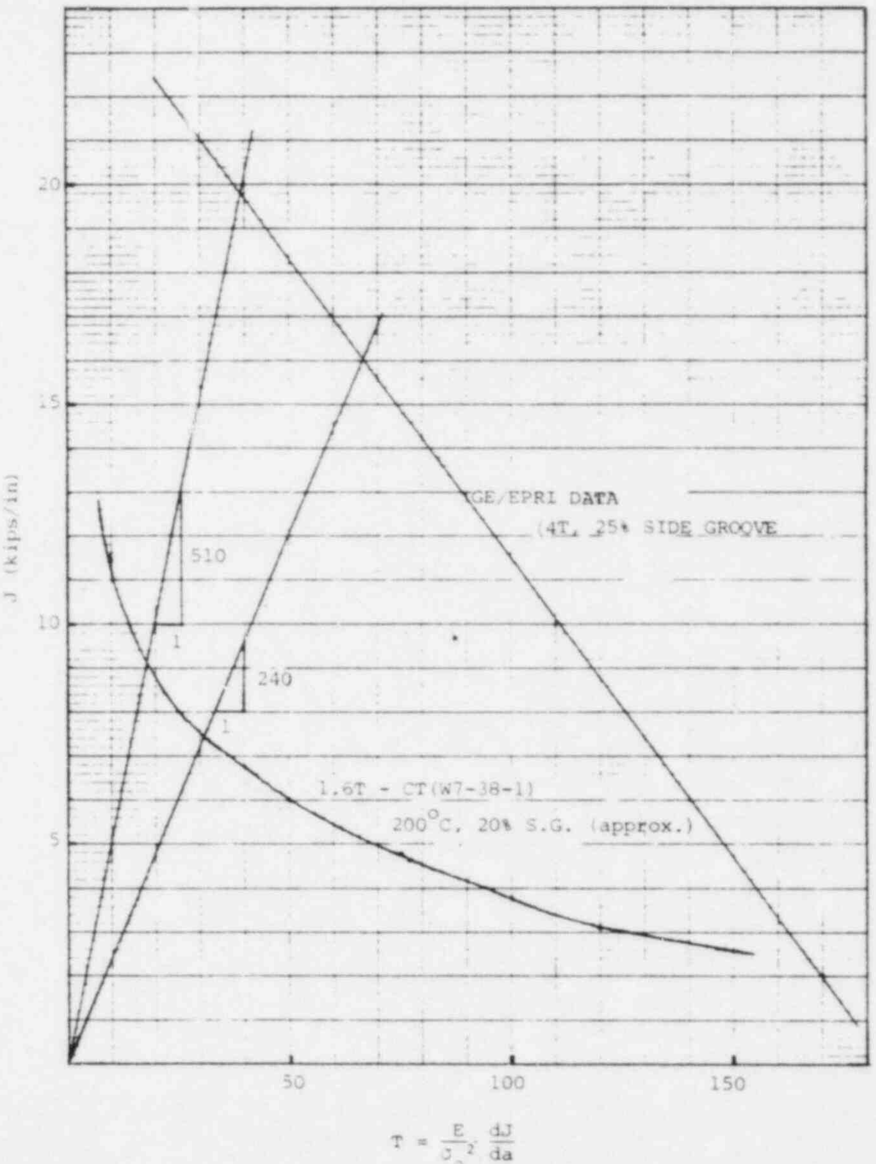


Figure 5.12 J Vs. T Curves for the Large Tensile Specimens Corrected for Plane Stress

where M and Q are factors used and defined in the ASME Code, Appendix G, LEFM analysis method.

The stress function $\{F(\frac{\sigma}{\sigma_{ys}})\}$ consists of constants for the elastic and plastic stresses. The sensitivity analysis was conducted to evaluate the dependence of the constants on the values of the stress-strain curve parameters. For plane-strain conditions, a relationship has been developed between $\left(\frac{\varepsilon_t}{\varepsilon_{ys}}\right)$ and $\{F(\frac{\sigma}{\sigma_{ys}})\}$, where ε_t is true strain.

The relationship appears to be consistent, regardless of material or experimental conditions. This relationship should be used to estimate the stress term.

The geometry correction term in Equation (5-19) included the parameters M^2 and Q. The variability of several solutions for M was evaluated for the surface flaw. For a standard (Appendix G) flaw with a/t = 0.25 and a/c = 0.17, a total variability of 11% was observed for M. A similar observation was made for Q. Therefore, the use of Q based on $\sigma/\sigma_{ys} = 0$ will provide a nonconservative value of J_{appl}. Combining the two effects suggested that the errors were compensating, which resulted in a reasonably accurate estimate of the geometry term.

The effect of deleting the geometry correction term from $\partial J/\partial a$ on the calculated value of T was determined. Comparison of $\partial J/\partial a$ calculated without the geometry term compared to $\partial J/\partial a$ with the term resulted in an overestimate of 24%. The deletion would result in an overestimate of T_{matl} by 24%.

5.4.2 Comparison With Other Criteria

The method for reactor pressure vessel analysis based on the J-integral and tearing modulus concepts was compared with other EPFM approaches (Appendix I). Generally, the J-T method provided similar results, but its range of applicability was the best of those examined. It was concluded that EPFM analysis using the structural and materials J-T curves provided a satisfactory basis for the resolution of the safety issue of low USE RPV steels at relatively high temperatures.

An alternate approach available through the British Standards Institute (BSI)[18] was used to calculate the maximum allowable stresses for an Appendix G-type of defect. This approach requires that $J_{appl} \leq 0.7\ J_{crit}$. Depending on the value of C_v energy (35 or 50 ft-lb) and σ_{ys} (80 or 97 ksi), the maximum allowable circumferentially oriented stress varied from 24.5 to 28.8 ksi. A comparison of calculated maximum allowable stresses was conducted using the BSI approach and the elastic-plastic equations for J and T previously presented, after first multiplying the EPFM allowable stresses by 0.7 to bring both to a similar factor of safety. A ratio of the maximum allowable stress from the first to the second approaches varied from 1.12 to 1.03. The closeness of the two approaches is surprising, because in addition to the difference in fundamental approaches, different correlations were used relating C_v to K or J. It was concluded that the elastic-plastic fracture mechanics analysis using the structural and materials J-f(T) curves provided a satisfactory basis for the resolution of the safety issue of low USE RPV steels at relatively high temperatures.

5.5 EPFM/E_{C-V} Correlation

Some RPVs have neither specimens which could be used to generate J-R curves nor available archival material from which the curves could be made. Even if such a situation applies to only one material of construction (for example, one of the welds), it would be enough to create an impasse relative to using the foregoing elastic-plastic analysis. Therefore there is a need for an indirect means of determining the relevant mechanical parameters for the irradiated steel in question. By pulling together several observations which grew out of the TAP A-11 work, it was possible to produce a potentially useful correlation between values of J which would provide a conservative estimate of unstable fracture and the corresponding C_v USE. The nature of the correlation is described in the following paragraph.

Fracture tests based on the single specimen unloading compliance procedure resulted in J-R curves which exhibited continuously changing slope, not unlike the familiar true-stress/natural-strain tensile curves (Appendix D). On that basis: (1) the tearing modulus, T, would be a continuously changing parameter and(2) data reduction to curves of J = f(T) would result in an hyperbola. Most importantly, the hyperbolic materials curves were observed to form a family of parametric curves, generally increasing in magnitude with the USE from C_v tests of the same material. Loading curves of $(J/T)_{appl}$ for RPVs with cracks were determined to be straight lines out of the origin with slopes of the order 500 in.-lb/in.^{2} Thus, a loading curve of slope J/T = 50 in.-lb/in.^{2} would be a <u>very</u> conservative estimate of the cracked RPV behavior. Most of the hyperbolic material curves would cross the $(J/T)_{50}$ loading line within the range of valid experimental measurements, whereas either extrapolation would be necessary or small values of ω would occur in order to reach loading lines of slope J/T = 500 in.-lb/in.^{2}, or so. When values of J at the intersections of the hyperbolic material curves with the J/T = 50 in.-lb/in.^{2} loading line were plotted against the corresponding C_v USE (Appendix D), a reasonably narrow, generally parabolic, scatter band was obtained (Figure 5.13, Appendix B). The lower bound curve of the scatter band was populated with data from all varieties of material: plates, forgings, and welds, both irradiated and nonirradiated. In the absence of J-R curves for the actual RPV material(s), the lower bound of Figure 5.13 can be used to obtain a conservative value of J to be used in fracture instability calculations, along with T = J/50. As additional data become available, the lower bound curve should be modified.

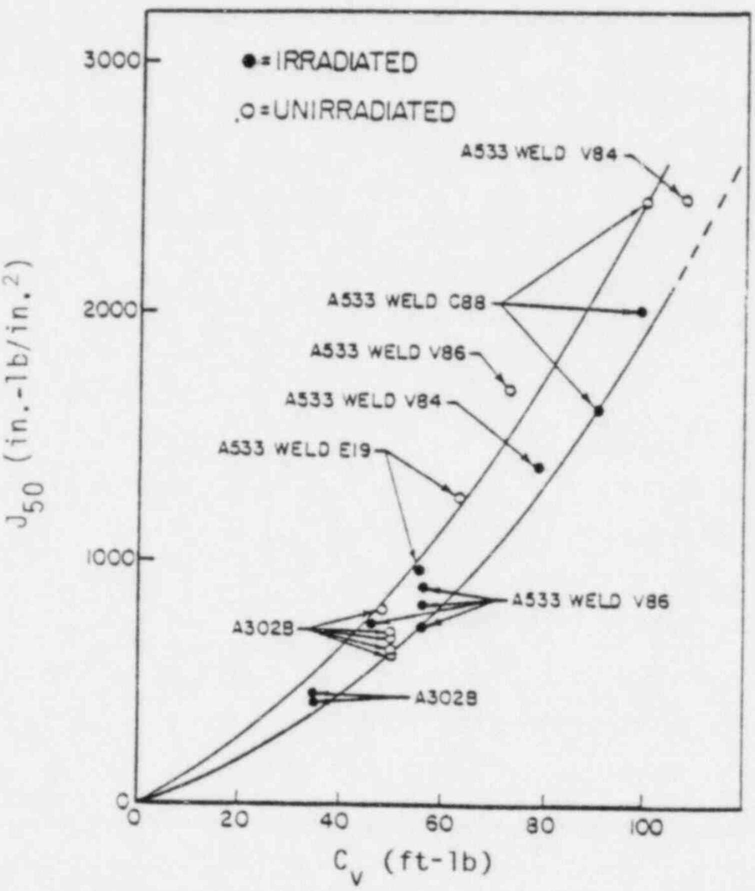


Figure 5.13 Correlation between J (at the intersection of the J/T = 50 in.-lb/in.^{2} load line and a materials curve) and the corresponding Charpy upper shelf energy value

Underlying the problem of discovering useful correlations between mechanical property data is the larger issue of the applicability of data obtained on small specimens under laboratory conditions to full-scale pressure vessels under operating conditions. Clearly, experiments on compact (CT) specimens come closer to modeling RPVs than do C_v tests, yet the latter have been accepted historically by the design, operating, and regulatory communities. The majority of the J-R curves used in resolving the TAP A-11 issue were obtained from CT specimens with 20% side grooves.* Results for A 533-B steel have shown that 20% side grooves are required to produce a straight crack front extension. To the extent that properly side grooved specimens approximate plane strain tearing behavior, they should be directly applicable to problems involving propagation in the thickness direction of a surface flaw in a pressure vessel wall (at least until the crack approaches the back surface) and to a longitudinal propagation direction near the midthickness of a pressure vessel wall.

*Grooves with Charpy V-notch dimensions were cut to 10% of the thickness on each side, giving a total thickness reduction of 20%.

6 LICENSING ASPECTS

The staff concludes that the approach and methodology described in the following provide an acceptable means for all commercial nuclear power reactor licensees to meet the requirements of 10 CFR, Appendix G, with regard to the need to demonstrate adequate margins for continued operation when the requirements of Section V.B. (of Appendix G) cannot be satisfied.

In accordance with the requirements of 10 CFR, Appendix G, all licensees should take the following course of action. The USE at the plant-specific end of life (EOL) should be established in accordance with 10 CFR 50 and the ASME Code. If the EOL USE $\geq$ 50 ft-lb, the RPV is acceptable (other factors, detailed in 10 CFR and in the Code, remain in force). If the EOL USE $\leq$ 50 ft-lb, either a safety analysis should be performed to demonstrate that the RPV can operate with adequate margin or a thermal anneal should be performed to restore the RPV material toughness. To be acceptable, the analysis must show adequate margin under normal, upset, emergency, faulted, and test conditions. The analysis may follow either the method recommended by the NRC, as presented in this report, or a method of equal or better reliability. Licensees who follow the NUREG-0744 approach should establish J-T curves for all materials in the RPV, either from experimental J-R curves or by correlation with the lower bound J (at J/T = 50) = f(USE) curve.

To determine the margin of safety, upper shelf (high temperature) failure conditions can be calculated conservatively from the J and T values at the intersection of a relevant [J=f(T)] materials curve and the loading line of slope: J/T=50 in.-lb./in.^{2}, and compared to the values of the elastic-plastic fracture mechanics parameters based on plant conditions.* Clearly, it is unacceptable to have the value of J (or T) derived from plant conditions be equal to or greater than the calculated J (or T) at fracture instability. To ensure safety, the parameter representing fracture instability conditions must be significantly greater than the parameter derived from plant conditions. The problem of quantitatively establishing an adequate and generally acceptable

*The safety margin can just as well be determined by comparing the calculated fracture stress (or pressure) to the stress (or pressure) derived from the plant conditions. When so determined, it will be roughly the square root of the margin based on J (or T).

safety margin for all plant conditions has not been resolved. The criteria presented in the "For Comment" version of NUREG-0744 were not generally acceptable (see Part II). Some commentors submitted carefully devised alternatives, but to revise this chapter by incorporating them would have either delayed the completion of Task A-11 by the time needed to go through a second "For Comment" edition or adopted criteria without adequate peer review. Instead, a three-part strategy was developed. First, Sub-Task C (page A-2, NUREG-0744) was deleted in principle as a resolution requirement. Second, a formal request was submitted* to the ASME Code Committee to develop tentative safety margins. Third, when that work is done, the NRC will review it and, if approved, will adopt the results by reference, as much of the Code is now handled. Initial indications, reported in the meeting minutes of the ASME Section XI Working Group on Flaw Evaluation for May 11, 1982, at San Diego, California, showed that an adequate margin can be demonstrated for calculated failure conditions based on the $J/T = 50$ in.-lb/in.^{2} and materials curves intersection. For normal and upset conditions (Levels A and B), the margin based on elastic-plastic calculations must be no less than that now required by the ASME Code. The NRC staff will hold the establishment of quantitative margins for emergency and faulted conditions (Levels C and D) in abeyance pending completion of the Section XI task. Until such time as the margin requirements approved by the ASME Code Committee are adopted by reference into 10 CFR 50, the NRC staff will review submittals under Section V.B, Appendix G, 10 CFR 50, on a case-by-case basis. Specific evaluations will depend on safety analysis details; the determination of adequacy must depend in large measure on the supporting engineering analysis. For an analysis based on an assumed Appendix G flaw and the lower bound curve of Figure 5-13, an adequate margin should be a ratio of J at instability (J_{50}) to the J calculated from plant conditions no less than 1.2 (a 20% margin based on J-integrals). The known conservatisms inherent in assuming that (1) the flaw depth is one-fourth of the RPV wall thickness, and (2) the loading curve is about one-tenth the slope of the actual vessel curve add confidence to the judgment of adequacy. If other factors in the safety analysis prove to be significantly unconservative, they

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*Letter to L. T. Chockie from R. E. Johnson, dated April 20, 1982.

could jeopardize that judgment. If the safety margin for some operating conditions is unacceptable, the licensee may opt to modify the plant system, plant operations, or both, to ensure that potentially damaging conditions are avoided.

The staff has concluded that there is no need to modify 10 CFR 50, Appendix G or H. NUREG-0744 does not result in any new requirements for the nuclear industry. The requirements in 10 CFR 50 remain unchanged, and this report provides one acceptable (and recommended) way of meeting one of those requirements.

The procedures of Appendix G of the ASME Code are applicable only to normal and upset operating conditions (Levels A and B). With respect to Levels C and D, Section III states:

> The possible combinations of loadings, defect sizes, and material properties which may be encountered during Level C and Level D limits are too diverse to allow the application of definitive rules and it is recommended that each situation be studied on an individual case basis.

One of the major reasons for not including such rules was the recognized need to consider relevant upper shelf material properties and analytical methods which were unavailable in 1972. Obviously this is a situation which has not been fully covered; further discussion is presented in Appendix J of this report.

Appendix G to 10 CFR 50 essentially adopts the ASME Code Appendix G, with additional restrictions related to the presence of fuel or of criticality. The additional restrictions will not be discussed here because they apply in the lower region of the brittle-ductile transition. However, 10 CFR 50, Appendix G, extends the applicability of the design rules to operations, and fluence effects must be considered. Because the resulting pressure/temperature limitations must be included in the Technical Specification which controls plant operation, the 10 CFR 50 Appendix G rules apply to all operating plants.

Because normal and upset conditions are limited by both Section III, Appendix G, and Section XI rules, a planned operation can be controlled in a manner which contributes to safety. In contrast, events which may occur during the initial stages of an emergency or a faulted condition are, by definition, out of control. Brittle fracture prevention considerations, although of obvious importance to plant safety, are not necessarily the only basis for operator action under accident conditons. However, it may be useful to limit plant recovery operations, such as limiting the system repressurization following a main steamline break (MSLB) by having the operator take action 10 or so minutes after initiation of the event. The most important goal of limitations on emergency or faulted conditions is assurance that the plant can be shut down and maintained in a shutdown condition. Additionally, for emergency conditions, it is desirable that the damage resulting from the event be repairable.

The need to include rules for emergency and faulted condition control in the ASME Code, Section III, Appendix G, is not clear. The Section III rules are of value only to the extent that they influence the construction (that is, materials, design, fabrication, examination, testing, and certification), and it is not apparent that such rules would have that effect. Although material selection might be influenced, indications are that the current acceptance criteria are satisfactory in that they provide adequate lifetime fracture resistance. Any major changes in the criteria would eliminate materials with which there is about 1000 reactor years of operating experience. Therefore, application of Task A-11 concepts to new plants should conform to existing procedures for material selection and qualification.

In contrast, inclusion in Section XI is essential for inservice flaw indication evaluations and to account for the effect of fluence on material properties. Such an action also has the advantage of utilizing the actual material properties for all evaluations, a possibility which Section III does not provide. It appears advisable, therefore, to include rules for evaluation of emergency and faulted conditions in nonductile failure prevention only in Section XI.

7 ANCILLARY ASPECTS

7.1 Neutron Fluence

The mechanical properties which are used in calculating high temperature safety margins of RPVs with relatively low USE are known to depend on exposure to neutron radiation. The A-11 Task Action Plan (Appendix A) originally included a Sub-Task (item 2.E, page A-2) directed toward two problems. One was the consideration that actions such as core redesign or even revised fuel management procedures could reduce the neutron flux at the RPV wall, thereby slowing down the embrittlement rate. Another was the prospect of realizing some relief by virtue of more precise fluence calculations if the result would be a net decrease in RPV irradiation. The two problems are discussed in the same order in this section.

Cursory evaluation of the level of flux reduction that could be achieved by application of core redesign, etc., led to the conclusion that it was not worth pursuing as part of Task A-11. For one thing, the level of effort and time required to complete such a study would significantly delay the resolution of the unresolved safety issue. For another, because no more than about one-fourth of the operating plants have steel that will reach the 50 ft-lb level of USE by end of life, barring detrimental changes in operating practice, the study would be of limited value. While that is true for Task A-11, fluence reduction is one of the proposed corrective actions in the long-term program for USI A-49, "Pressurized Thermal Shock." The impact of the work on Task A-49 could be substantial because of the number of plants involved and the material parameter (transition temperature), so the work proposed in A-11 will be done as part of A-49.

The second problem in Sub-Task E of USI A-49 was to determine if the safety issue could be ameliorated, if not resolved, by improving the accuracy in calcuations of fluence as a function of location in the vessel. There are some disturbing aspects to this problem both with respect to the theoretical calculations (neutronics) and the experimental determinations (dosimetry). A large, long range effort is under way, sponsored by the NRC Office of Regulatory Research (RES). The total neutronics/dosimetry problem, like the flux reduction problem, is beyond the scope of TAP A-11; however, it is recognized that establishing the relevant fluence in an RPV, like the task of determining the relevant stress, must be performed by the licensee with all the accuracy and reliability warranted by the state of the art, because both become inputs to the solution to the TAP A-11 problem.*

Reactor physics codes have been written to predict the neutron spectrum and flux level in reactor surveillance and vessel wall environments. Methods, materials, and equipment are available to measure the instantaneous neutron flux and subsequent time-integrated fluence that impinge on a surveillance capsule. The predictive codes and dosimetry measurement methods, however, have been developed independently over the years and, when used together in analyses, they do not necessarily give accurate answers. With reasonable effort, the calculational and experimental methods can be internally consistent and calibrated to yield an accuracy of ± 15%.** Neutron dosimetry predictions and measurements for application to surveillance programs are being upgraded and standardized. A comprehensive, vigorous research program under NRC sponsorship already has provided significant improvements. Some of the activities in the program include the following:

(1) Reactor physics calculations can be certified by referencing to benchmark flux or spectrum calculations.

(2) Dosimetry counting measurements and fluence derivations can be certified through a satisfactory comparison of results from a test set of dosimetry foils to the results from a standard set of surveillance capsule foils.

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*The material presented in the following paragraphs is discussed in detail in Appendix G.

**With extensive effort, an accuracy of ± 10% probably could be achieved; today's routine practice is more like ± 20%, at one standard deviation.

(3) The improvement gained from use of displacement per atom (dpa) as an exposure or damage parameter rather than fluence >1 MeV has been demonstrated.

(4) Exvessel dosimetry (between the vessel wall and the biological shield) can be used in conjunction with surveillance programs.

(5) Existing ASTM standards have been improved and the development of new ones undertaken in a general plan to provide the means for accurate, reliable reactor vessel neutron surveillance dosimetry.

Calculations of RPV margins require material property values as inputs which, in turn, depend on the exposure to neutron radiation. The problem of ensuring proper radiation values is beyond the scope of Task A-11. It is expected that licensees and others who apply the recommended fracture mechanics engineering analyses will couple the effort with state-of-the-art fluence determinations.

7.2 Pressure Vessel Data

Because material property data are necessary for evaluating RPV integrity, TAP A-11 included a subtask of developing a computer-based program for storage and retreival of operating RPV data and a computerized system to accomplish the storage and retrieval. The program is called the materials surveillance information computer system (MATSURV) and is described in Reference 19.

The system can cross reference between RPV materials, surveillance materials, and irradiated and unirradiated data, allowing quick identification of the limiting materials in any operating RPV and correlation of those materials with pertinent surveillance program specimens and test results.

MATSURV also includes data on operating RPV materials of fabrication, surveillance specimen materials and types, and results of pre- and post-irradiation tests of surveillance specimens. General information for each plant includes plant name and unit number, the nuclear steam supply system vendor, the RPV manufacturer, reactor type, EOL fluence and fluence rate, RPV design conditions, and RPV base metal specifications. Typical examples of the data stored for RPV plates, forgings, and welds are the manufacturer's identification, steel producer, heat numbers, weld wire specification, flux type and grade, location in the RPV, EOL fluence, fluence rate, RT_{NDT}, heat treatment sequence, and chemical composition.

The staff intends to ensure the correctness and completeness of the MATSURV data, to provide the software necessary to perform safety-related RPV calculations using the MATSURV data, and to provide access to MATSURV for the nuclear industry or other interested segments of the public.

7.3 Pressure Vessel Annealing

Also at issue is the question of how an operating plant should be annealed. Details can be found in Appendix F; only brief comments are appropriate here.

Appendix G of 10 CFR 50 identifies thermal annealing as one method that may be used to restore material toughness to acceptable levels for continued operation. However, little field experience and few research results are available to help define precisely the variables that will result in the most efficient and safe annealing process. Nonetheless, although test results are limited, they are sufficient to indicate that annealing at 650°F and 750°F can restore upper shelf fracture toughness and maintain the levels above those required to comply with 10 CFR 50.

Programs sponsored by the NRC and Electric Power Research Institute (EPRI) have established that in situ RPV annealing is feasible. At the same time, the conditions (for example, maintaining 750°F for one week) suggest a difficult, costly operation. The decision to anneal will demand a thorough, indepth, engineering study. Attention must be paid to potentially deleterious side effects such as concrete degradation. The evaluation must include the constraints imposed on the licensee to achieve worker radiation protection at the reactor site. Environmental protection considerations will be important because to effect a 750°F anneal, the vessel internals must be removed, plant modifications may be necessary, and radioactive particulate matter may be dislodged and become airborne because there will be no primary coolant (water) present (only air or some other gas). In addition to ensuring that plant personnel and the general public are protected from accidental releases of radioactive fission and corrosion products during the annealing process, adequate consideration must be given to occupational radiation exposure, radioactive waste processing, radioactive material decontamination, and radioactive waste shipments which result from the reactor vessel annealing operation.

A single generic annealing process cannot be defined. The annealing process which would restore adequate upper shelf toughness most effectively depends on many variables and must be designed for each individual plant after a thorough engineering and safety evaluation. The variables that must be considered include the neutron fluence level at annealing, the desired period of subsequent operation, personnel exposure, fuel storage capacity, system heat removal capacity, protection of safety-related systems during annealing, the time period the annealing process will remain effective under subsequent irradiation, and verification of the annealing effectiveness. Whatever method is chosen by the licensee, it must be performed under the guidelines of keeping releases of radioactivity to the atmosphere as low as reasonably achievable (ALARA guidelines) and according to the procedures contained in Regulatory Guide 8.8.

8 REFERENCES

(1) P. C. Paris, et al., "A Treatment of the Subject of Tearing Instability," USNRC Report NUREG-0311, July 1977.

(2) U.S. Code of Federal Regulations, Title 10, Energy, Part 50, "Domestic Licensing of Production and Utilization Facilities."

(3) U. Potapovs and J.R. Hawthorne, "The Effect of Residual Elements on 550°F Irradiation Responses of Selected Pressure Vessel Steels and Weldments," NRL Report 6803, U.S. Naval Research Laboratory, Washington, D.C.

(4) J.R. Hawthorne, "Demonstration of Improved Radiation Embrittlement Resistance of A 533-B Steel Through Control of Selected Residual Elements," NRL Report 7121, U.S. Naval Research Laboratory, Washington, D.C.

(5) U.S. Nuclear Regulatory Commission, Regulatory Guide 1.99, "Effects of Residual Elements on Predicted Radiation Damage to Reactor Vessel Materials," Revision 1, April 1977.

(6) American Society of Mechanical Engineers, Boiler and Pressure Vessel Code, Sections III and XI.

(7) J.G. Merkle, G.D. Whitman, and R.H. Bryan, "An Evaluation of the HSST Program Intermediate Pressure Vessel Tests in Terms of Light-Water Reactor Pressure Vessel Safety," Oak Ridge National Laboratory Report ORNL-T-5090, November 1975.

(8) Welding Research Council, "PVRC Recommendations on Toughness Requirements for Ferritic Material," WRC Bulletin 175, by the PVRC ad hoc Task Group on Toughness Requirements, August 1972.

(9) J. W. Hutchinson, Journal of the Mechanics and Physics of Solids, Volume 16, 1968, pp. 13-31; J. R. Rice and G. F. Rosengren, Journal of the Mechanics and Physics of Solids, Volume 16, 1968, pp. 1-12.

(10) P. C. Paris and H. Tada, "Further Results on the Subject of Tearing Instability," USNRC Report NUREG/CR-1220, Volume I, January 1980; H. Tada and P. C. Paris, "Further Results on the Subject of Tearing Instability," USNRC Report NUREG/CR-1220, Volume II, January 1980.

(11) H. Tada and P. C. Paris, "Tearing Instability Analysis Handbook," USNRC Report NUREG/CR-1221, January 1980.

(12) H. A. Ernst and P. C. Paris, "Techniques of Analysis of Load-Displacement Records by J-Integral Methods," USNRC Report NUREG/CR-1222, January 1980.

(13) P. C. Paris, "CSNI Specialists Meeting on Plastic Tearing Instability," USNRC Report NUREG/CR-0010 (also CSNI Report No. 39), January 1980.

(14) F. J. Loss, ed., "Structural Integrity of Water Reactor Pressure Boundary Components, Quarterly Progress Report, April-June 1980," USNRC Report NUREG/CR-1783, February 1981 (also NRL Memorandum Report 4400).

(15) P. C. Paris, H. Tada, A. Zahoor, and H. Ernst, "The Theory of Instability of the Tearing Mode of Elastic-Plastic Growth," in Elastic-Plastic Fracture, ASTM STP 668, 1979.

(16) J. E. Srawley and W. F. Brown, Jr., Fracture Toughness Testing and Its Applications, ASTM STP 381, American Society of Testing and Materials, 1965.

(17) V. Kumar and others, "Estimation Technique for the Prediction of Elastic-Plastic Fracture of Structural Components of Nuclear Systems," Fourth Semi-Annual Report, EPRI Contract RP-1237-1, Electric Power Research Institute, Palo Alto, CA, September 1981.

(18) British Standards Institute, "Guidance on Some Methods for the Derivation of Acceptance Levels for Defects on Fusion Welded Joints," PD-6493:1980, BSI, London, 1980.

(19) J. Strosnider and C. Monserrate, "Computerized Reactor Pressure Vessel Materials Information System," USNRC Report NUREG-0688, October 1980.

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U.S. NUCLEAR REGULATORY COMMISSION

BIBLIOGRAPHIC DATA SHEET

1. REPORT NUMBER (Assigned by DDC)
NUREG-0744, "For Comment"

4. TITLE AND SUBTITLE (Add Volume No., if appropriate)
"Resolution of the Reactor Vessel Materials Toughness Safety Issue"

2. (Leave blank)

3. RECIPIENT'S ACCESSION NO.

7. AUTHOR(S)
Richard E. Johnson, and others

5. DATE REPORT COMPLETED
MONTH: April | YEAR: 1981

9. PERFORMING ORGANIZATION NAME AND MAILING ADDRESS (Include Zip Code)
U. S. Nuclear Regulatory Commission
Division of Safety Technology
Washington, D. C. 20555

DATE REPORT ISSUED
MONTH: September | YEAR: 1981

6. (Leave blank)

8. (Leave blank)

12. SPONSORING ORGANIZATION NAME AND MAILING ADDRESS (Include Zip Code)
U. S. Nuclear Regulatory Commission
Division of Safety Technology
Washington, D. C. 20555

10. PROJECT/TASK/WORK UNIT NO.

11. CONTRACT NO.

13. TYPE OF REPORT
Regulatory

PERIOD COVERED (Inclusive dates)

15. SUPPLEMENTARY NOTES

14. (Leave blank)

16. ABSTRACT (200 words or less)

The central problem in the Unresolved Safety Issue A-11, "Reactor Vessel Materials Toughness," was to provide guidance in performing analyses for reactor pressure vessels (RPVs) which fail to meet the toughness requirements during service life as a result of neutron radiation embrittlement. A technical team of recognized experts was organized to assist the NRC staff in addressing the problem. Using the foundation of the tearing modulus concept, which has been developed under earlier NRC sponsorship, relationships were obtained which provided approximate solutions to the RPV fracture problem with assumed beltline region flaws. Volume I of this report is a brief presentation of the problem and the results; Volume II provides the detailed technical foundations.

17. KEY WORDS AND DOCUMENT ANALYSIS

17a. DESCRIPTORS

Reactor Vessel Materials Toughness

17b. IDENTIFIERS/OPEN-ENDED TERMS

18. AVAILABILITY STATEMENT
Unlimited

19. SECURITY CLASS (This report)
Unclassified

20. SECURITY CLASS (This page)

21. NO. OF PAGES

22. PRICE
$

NRC FORM 335 (7-77)

Part II

Part II

Staff Responses to Public Comments

Part II

Contents

LIST OF COMMENTORS

Public comments on the "For Comment" issue of NUREG-0744, dated September 1981, were received from the individuals and organizations listed below. Although the public comment period was officially terminated at the end of November 1981, comments received later were accepted, mainly because of confusion and delay in getting review copies through the mails to people who had requested them. The comments and their resolutions can be found on the following pages of Part II of NUREG-0744.

Mr. Kenneth I. Baron, ASME Boiler and Pressure Vessel Code Committee, Section XI, New York, NY

Mr. Arthur C. Bivens, Atomic Industrial Forum, Washington, DC

Mr. Richard M. Eckert, Atomic Industrial Forum, Washington, DC

Dr. Frank J. Loss, Naval Research Laboratory, Washington, DC

Mr. E. P. Rahe, Jr., Westinghouse Electric Corporation, Pittsburgh, PA

Mr. B. F. Saffell, Jr., EG&G Idaho, Inc., Idaho Falls, ID

Mr. A. E. Scherer, Combustion Engineering, Inc., Windsor, CT

Mr. J. H. Taylor, Babcock & Wilcox Company, Lynchburg, VA

Additionally, the members of the A-11 Technical Team (see Part I, p. vii) made substantial contributions to the final (Rev. 1) NUREG.

PART II

STAFF RESOLUTION OF PUBLIC COMMENTS*

Comment 1: (General) The NUREG, with some minor wording changes and an emphasis on the responsibility of the analyst to use or develop proper methods and criteria, will be a very useful document. Then it would truly represent a Resolution of the A-11 issue.

Resolution: The staff agrees and has made a considerable effort to accommodate all suggested changes; the compliment is deeply appreciated.

Comment 2a: (General) While the NUREG should be helpful as a guide to future vessel analysis, the procedures appropriate for these analyses are still evolving. Thus, it is premature for this NUREG to propose specific analysis procedures of specific evaluation criteria. It is therefore recommended that NUREG-0744 be modified before publication to eliminate the implication that the specific analysis procedures and evaluation criteria discussed in the report are the only acceptable ones.

Comment 2b: (General) The development of simplified approaches should not preclude the use of more detailed analyses which could show additional margin. In using a simplified approach, much of the essence of the problem is lost or buried in nondimensional parameters. To base a licensing decision only on such procedures would be inadvisable at this time.

Comment 2c: (General) The method of analysis described in this report should not be viewed as the only acceptable method for such an integrity analysis. Because this method is new and untested except for the examples considered, it is suggested that this document be considered "A Recommended Methodology for the Resolution of the Reactor Vessel Materials Toughness Safety Issue."

Comment 2d: (General) NUREG-0744 should be modified before publication to eliminate the implication that the specific analysis procedures and evaluation criteria discussed in the report are the only acceptable ones.

Comment 2e: (General) A major concern is the tacit acceptance of the specific elastic-plastic methods presented in the NUREG without sufficient proof of applicability to real vessels. The J-T methods described are not amenable to combined thermal and pressure loading, nor do they directly account for the effects of stable crack extension. As a first approximation, the simplified methods provide some guidance

*Page numbers and paragraphs cited refer to those in the original issuance of NUREG-0744.

for determining the behavior of a flaw in a vessel under limited types of loading. However, to recommend the use of such methods for evaluating the more general situation is premature and without sufficient experimental or analytical justification.

Comment 2f: (General)

NUREG-0744 as issued for comment does not provide resolution of the Reactor Vessel Materials Toughness Safety Issue.

The NUREG is responsive to Subtasks A, B, and F as listed under Item 2, Plan for Problem Resolution, in Appendix A of the NUREG. Also, the NUREG responds to Subtasks D and E of that listing to the extent expected in a general report. The failure is in accomplishing Subtask C of that listing: "(u)sing the results from Subtasks A and B, define reactor vessel safety criteria to avoid failure by tearing instability fracture, to supplement existing criteria for other failure modes." Subtasks A and B have not been assimilated into safety criteria which are sufficiently specific to provide meaningful and consistent resolution of this issue.

The basic issue can now be resolved by revising a 10 CFR 50 requirement which was developed in an arbitrary manner about 1970. This requirement, that the material attain a Charpy V-notch upper shelf energy of at least 50 ft-lbs, can now be shown to be arbitrary and overly conservative.

The 50 ft-lb value required by the present 10 CFR 50 Appendices G and H, or the published proposed revision to these appendices, is an arbitrary and conservative value developed about 1970 in recognition of the concern about failure by tearing. The 50 ft-lb value was never considered to be related in any way to the now outmoded, but then active, transition temperature method of brittle-fracture prevention. The NUREG provides the necessary information to formulate a revision to the 50 ft-lb requirement which is conservative but is based on technical considerations rather than emotion.

Specifically, for a typical PWR RPV vessel of radius 10 times the thickness subjected to an operating pressure of 2250 psig, a tentative proposal has been developed which would replace the quantity 50 ft-lb in the proposed 10 CFR 50 revision with a thickness-dependent value defined by

Required minimum C_v USE = 35 + 5(t-6) but $\leq$ 50 ft-lb, where t is the thickness of the vessel beltline region in inches.

The proposed values could be presented in tabular form, as follows with interpolation permitted.

Beltline Thickness (in.):	≤ 6	7	8	≥ 9
Required C_v USE (ft-lb):	35	40	45	50

The proposed values are in excess of the values required, using the lower bound properties and methods of the NUREG, to ensure:

- o Stability (leak-before-break) with a through wall flaw of length equal to twice the vessel thickness (D.4.a)

- o A factor of safety of 2 on initiation from an Appendix G (t/4 by 3t/2) surface flaw (D.4.c)

- o A factor of safety of 10 on the initiation of an $a/\ell = 1/6$ surface flaw of height $a/t = 0.050$, twice the height permitted for acceptance by examination by Section XI (D.4.e)

- o A factor of safety of 4 on instability from an Appendix G (t/4 by 3t/2) surface flaw (D.4.c)

In addition, the proposed values are not inconsistent with pressure vessel operating experience.

Acceptance of the proposed values would significantly increase the operating period before which, if ever, the C_v USE values would drop to a level below that permitted by 10 CFR 50.

Simply stated, the basic problem is an overly conservative regulation, not how to perform an analysis when the regulatory "magic number" is violated.

<u>Resolution</u>: The staff agrees with the central themes of the several commentors; i.e.: (1) the recommended analytical procedure, being based on some relatively new developments in elastic-plastic fracture mechanics, has not been tested extensively and (2) the recommended analytical procedure is not the only one which could be used to comply with the safety analysis requirements in 10 CFR 50. At the same time, it is recognized that opinion on what constitutes a sufficiency of testing in order to achieve general acceptability covers a broad spectrum. If one holds that there is adequate conservatism in basic reactor pressure vessel design when it incorporates linear elastic fracture mechanics, then the elastic-plastic refinement recommended in NUREG-0744 is not a gross departure from what has worked successfully thus far. Despite the shortcomings such as inadequate treatment of thermally induced stresses, the ability of the method to predict the failure conditions of the ductile steel in the HSST vessel burst tests bodes well for its applicability to steels embrittled by neutron irradiation. As for the correct argument that the recommended procedure is not the only one, a concerted effort was made to edit the NUREG to avoid such an implication. Hopefully the effort succeeded, but in case some oversight remains; let it be a matter of record that the NRC prescribes only the safety goals to be achieved and not the means to achieve them. It must be understood, however, that the staff is familiar and comfortable with the recommended procedure, and licensee submittals that follow it will be reviewed most efficiently.

Comment 3: (General) In several locations statements are made that LEFM is "not applicable" to the problem at hand. In general, LEFM will provide a conservative result if used properly, and the J-T method is designed to show added margin. It appears from the sample calculations provided that the J-T method as proposed provides only a slight improvement over strict LEFM methods.

It should be clearly stated that the proposed J-T methodology is designed to replace LEFM calculations only in the upper shelf regime of toughness, and that LEFM can and should still be used in the transition temperature regime and below.

Resolution: The contention that LEFM can be used properly with respect to the high temperature fracture problem cannot be supported. To use LEFM one must have valid K_{Ic} data and no such data exist (see Comment 32b). The assertion that LEFM would provide a conservative approximation to the ductile fracture problem has been bandied about in the fracture mechanics community for decades without resolution. The analytical procedures advanced in NUREG-0744 now provide a basis with which to assess that assertion. Even though the J-T methodology is itself an approximation, the more exact three-dimensional elastic-plastic analysis by H. G. deLorenzi provided a basis for comparison that showed the approximation worked extremely well (see NUREG-0744, Figure 5.6, pg 5-11). With respect to the admonition that the recommended method is to be applied only in the upper shelf regime of toughness, the staff trusts that the limitation is, indeed, clearly understood. As to the applicability of LEFM, it should be used at low temperatures (below the ductile-to-brittle transition), but there is a poorly understood upper limit, also, which may not overlap, or even join with, the region where J-R curves are used. At the least, the upper limit can be no higher in temperature than the high temperature limit of valid K_{Ic} data. Some extrapolation upward may help, but the staff recognizes that some engineering judgment may be required to bridge a gap in reliable calculations in the region of the "knee" where the transition region blends into the upper shelf region. Clearly, more work will be welcome.

Comment 4a: (General) The report should contain example calculations to demonstrate the use of the method. Without such calculations the implications of the proposed methods for actual vessels are totally unexplored. Vessels with both high and low shelf energies should be included.

Comment 4b: (General) The implications of the use of this method on a typical reactor vessel have not been explored--an example would be very useful. This would make the method easier to explain and allow a more complete assessment of its applicability.

Resolution: Example problems would be interesting, although they are more in the province of Regulatory Guides (or textbooks) than in the document covering the resolution of a generic issue. One reason for not addressing RPV example calculations in the original issue of NUREG-0744 was because it was believed that there was too much plant-to-plant variability in the application of the method, and one set of parameters could lead to a mistaken sense of security (or dread, depending on the outcome). Section XI of the ASME Boiler and Pressure Vessel Code Committee, specifically the Working Group on Flaw Evaluation, has taken action in response to the NRC request discussed in Chapter 6 of the revised NUREG. The Working Group is in the process of developing a plan for the establishment of accident-level safety factors. Toward that end, the method set forth in the NUREG was followed by Merkle* to prepare sample calculations that are being published in NUREG-0939. The commentors and other readers are referred to the reference cited as an example of the application of the method. Also, see the resolutions to Comments 5,6b, and, most important, 12.

Comment 5a: (General) It should be clearly stated which loading conditions the report is meant to treat, and which it is not intended to treat. It appears that the methodology is meant for application to normal, upset, and test conditions. If the methodology is meant to be used for faulted conditions, the report must be expanded to demonstrate applicability to thermal stresses, and to provide guidelines on acceptability criteria. The consideration of both normal and faulted conditions, which continues through Volume 1, is confusing.

Comment 5b: (General) The method is suggested to have general applicability to reactor vessel integrity, but it has not been applied to thermal stress situations. This seems to be a serious drawback and should be addressed.

Comment 5c: (General) Pg 6-1: The methodology is proposed for application to normal and upset conditions, so it is unreasonable to set criteria for faulted conditions, as stated here. The method does not deal with thermal stresses, the major stress component present in faulted conditions. The second paragraph is inconsistent with the first one, in that the method is first suggested as an acceptable approach, but then treated as the required method.

Resolution: The recommended analytic procedure is intended for application to normal, upset, test, and accident conditions. The work of Section XI of the ASME Code will clarify that position. As for the treatment of thermal stresses, it was noted above that the methodology is weak on that issue. However, it is not necessarily true that thermal stresses must be treated if accident conditions are to be considered. The largest thermal stresses result from large decreases in temperature and once the RPV steel temperature is decreased sufficiently, LEFM techniques are applicable. That argument is found in the technical approach to the NRC Unresolved

*Dr. Merkle, of the Oak Ridge National Laboratory, was a member of the Task A-11 Technical Team and the author of Appendix C of NUREG-0744.

Safety Issue Task A-49, "Pressurized Thermal Shock." The importance of treating thermal and combined mechanical and thermal stresses in elastic-plastic fracture mechanics has not escaped attention. Pg B-34, NUREG-0744, discussed this aspect. Professor Paul C. Paris of Washington University, St. Louis, Missouri, who was a member of the A-11 Technical Team and the author of Appendix B, NUREG-0744, is currently devoting much of his time to the subject.

Comment 6a: (General) J-integral computation and analysis methods are evolving fairly rapidly and at present are far from being in a mature state. Therefore, the analysis methods should not be specified but left to the analyst to implement and defend. For example, the requirement that the loading curve have a slope of J/T = 50 is based on the availability of data today. The viability of the J approach is dependent on the development of data at higher J/T values. Limiting the analysis to J/T = 50 discourages such development and will seriously limit the usefulness of the J-approach.

Resolution: The above comment and the four following (6b through 6e) all relate to analytical or computational details of the recommended procedure. They will be dealt with separately.

The staff agrees with the first two sentences in Comment 6a; both subjects were discussed relative to preceding comments. Serious misunderstandings emerge in the third sentence. First, the procedure does not require that the RPV loading curve have a slope, J/T = 50 in.-lb/in.^{2} The choice of 50 in.-lb/in.^{2} was based on the observation that approximate loading curves for RPVs with radius-to-thickness ratios R/t ≅ 10 calculated out to J/T ratios of about 500 in.-lb/in.^{2}, so 50 in.-lb/in.^{2} looked like a prudent, conservative value. Note the numerical values in Comment 6b below. More importantly, from a physical viewpoint, are the facts that available experimental J-T data for low Charpy USE material actually intersected the J/T = 50 in.-lb/in.^{2} line, obviating the need for extrapolations, and the data up to the intersections could be judged valid on the basis of ω being large enough. Although the staff recommends rereading pg B-36 ff for the reply to Comment 6a, the emphasis derived from reviewing the applicable relationships may be worthwhile (see NUREG-0744 to define the terms). Remember that a value of 10 or more in ω is considered large enough to conclude that J-R data where it obtains (actually a value of 5 is enough) are valid and may be used in fracture problems. Thus it is noted:

$$T = \frac{E}{\sigma_o^2} \cdot \frac{dJ}{da}$$

and

$$\frac{J}{T} = \frac{J\sigma_o^2}{E(dJ/da)}$$

By definition: $\omega = \frac{dJ}{da}\frac{b}{J}$,

which should be 10 or more.

Substituting: $\frac{J}{T} = \frac{\sigma_o^2 b}{E\omega}$

or $$\omega = \frac{\sigma_o^2\, b}{E(J/T)}$$

Selecting values of $J/T = 50$ in.-lb/in.², a conservatively low flow stress of $\sigma_o = 60{,}000$ psi, and an elastic modulus of $E = 30 \times 10^6$ psi it is calculated $\omega = 2.4$ b in.

For a crack half-way through an 8-in.-thick RPV wall, the remaining ligament, b = 4 in. and $\omega \simeq 10$, Q.E.D.

It is concluded that the recommended procedure results in a valid application of current elastic-plastic fracture mechanics using the conservative loading line of $J/T = 50$ in.-lb/in.² for cracks up to (and even beyond) one-half-way through the wall of a PWR vessel.

Comment 6b: (General) For pressure vessels J/T is shown to be about 500 or greater for crack sizes and materials of interest. The two numerical examples in Appendix C give J/T = 1750 and 540. The statement on pg 5-22 lines 2 and 3 that a loading curve with J/T = 50 would produce very conservative results is supported by considering the effect of that conservative requirement on the results of the Appendix C examples. In the first example, $J_{app} = 905$, so $T_{app} \approx J/50 = 18$. Using the J_{50} vs. C_v curve of Figure 5.13, J_{50} for CVN = 50 ft-lb is about 600. Comparing them ($J_{app}/J_{50} = 1.5$ and $T_{app}/T_{50} = 1.5$) indicated that the crack is unstable. Similar conclusions are drawn from the second example. In Appendix C, both examples result in stable cracks for CVN = 50 ft-lb material, when the actual J/T applied values are considered.

Resolution: The example calculations support the NRC position and no further discussion is necessary. It is germane to restate that analyses along the above lines have been reported by Dr. Merkle (ORNL) to Section XI of the ASME Code Committee and more are in the works.

Comment 6c: (General) Pg 6-1, Line 19: Comment 6a addressed the effect of the requirement to consider J/T = 50. It is unlikely that this requirement will enable a successful analysis to be performed. The analyst must be given the opportunity to analyze the real vessel. Testing other than Charpy tests will probably be required to evaluate such analysis results and work is progressing in this area.

Resolution: The response to Comment 6a stated that J/T = 50 in.-lb/in.^{2} is not a requirement. The NRC not only wants to provide the analyst the opportunity to analyze the real vessel, but expects that each licensee, if and when required by the regulations in 10 CFR 50 to perform an analysis, will provide one which is applicable to the real, plant-specific RPV. As for performing tests other than Charpy, it is expected that each licensee who will be required to perform an analysis will obtain J-R curves for the RPV steels in his vessel that exhibit Charpy USE values of 50 ft-lb (or thereabouts). There are plants that will not be able to generate J-R curves, however, because they do not have fracture mechanics specimens nor the archival material from which they could be made. For those vessels, the recommended procedure given in NUREG-0744 includes a correlation enabling the fracture mechanics analyst to utilize the Charpy V-notch data required by 10 CFR 50.

Comment 6d: (General) It is not all that clear whether plane stress assumptions are reasonable.

Resolution: Errors resulting from the wrong choice in stress state (i.e., assuming generalized plane stress when conditions actually correspond to generalized plane strain or vice versa) will be small compared to other potential errors and conservatisms in the recommended procedure. For a material with a Poisson ratio $\nu = 0.3$, the difference in stress (or stress intensity factor) between the plane stress and plane strain states is less than 10%, and the error in strain energy (or in the J-integral) is less than 20%. For the class of problems being considered--which includes net section yielding--the loss of constraint associated with the assumed level of plastic flow makes the plane stress state the preferable one.

Comment 6e: (General) In addition, it is not clear where the f* factors of page 3-24 came from.

Resolution: Thanks are due the commentor for calling attention to the f* values reported on pg B-24. The author of Appendix B found that they are in error; correct values were printed in Appendix B to NUREG-0744, Rev. 1. The numbers were obtained from relationships reported by Hutchinson (Ref. 15 of NUREG-0744). Note that the values of f* and G* should be about the same; thus the product $\bar{\bar{\alpha}}\, G^* = 11.8$, Appendix B, pg B-26, for values of $\bar{\bar{\alpha}} = 1.115$ and $n = 9.7$ (pg B-25), continues the trend observed in the corrected f* results.

Comment 7: (General) The use of the J/T analysis diagram tends to obscure the physical behavior of the structure and, as such, makes the determination of safety margins difficult. For example, the method does not provide the amount of stable crack growth, or the factor of safety on load. On pg 4-1, one of the stated requirements for an applicable method is that it have "direct physical significance with respect to safety margin determined."

Resolution: In defense of the J-T diagram as presented in NUREG-0744, the following comments are made: The concept is the same in principle as the plasticity analysis proposed in the Electric Power Research Institute (EPRI) documents. That method can be found in EPRI NP-1735 and EPRI NP-1931 (Kumar, 1981). Both the EPRI approach and the procedure published in NUREG-0744 aim at the same goal: calculation of the point of instability (the onset of fast-running fracture) under conditions of large prefracture plastic deformation.

The EPRI method requires the determination of the conditions of tangency between curves of the J-integral as functions of crack length representing the structure and the material J-resistance curve. Note that at the point of tangency, the slopes of the structure and material curves are equal. The approach in NUREG-0744 involves determining that same point of equal slopes for the structural and material curves, but by doing so on a plot of J as a function of T, sets forth the task of finding the intersection of the curves rather than the more difficult, somewhat subjective, point of tangency. If the analyst has been schooled in fracture mechanics to the point of being familiar with crack resistance, or R-curves (see, for example, Broek, 1974), the physical behavior portrayed by the J-T diagrams will loose its obscurity. As for the determination of safety margins, the work done by Dr. Merkle for the ASME Code Committee, Section XI demonstrates that it is not difficult (perhaps tedious, but then stress analyses often are). The amount of stable crack growth can be determined, as Merkle showed, providing the structure behaves as predicted by small specimen J-R curves. The statement quoted was part of the statement of criteria to which the A-11 Technical Team was dedicated. The staff is of the opinion that the physical significance can be extracted from the method. As later discussion will show, the task of establishing safety margins has been set aside with respect to resolving the A-11 safety issue and the NRC will await the results of the ASME Code Committee review.

Comment 8: (General) Methods to enable consideration of thermal gradients and variations of J_{mat} and T_{mat} with temperature (e.g., to compute pressure-temperature operation curves when Appendix G is no longer applicable) are not yet available and will be necessary for proper application of the J concept to operating vessels. Development of such methods should be encouraged, or at least not discouraged, by the NUREG.

Resolution: It is true that neither the thermal stress effects nor the temperature-dependence of the materials parameters employed in the recommended procedures have been extensively researched.

Thermal stresses were discussed in regard to Comments 5a and 5b, above. The need for further analyses to fully account for non-uniform stress fields was discussed in NUREG-0744, Appendix B, pg B-34. The need is not so great as to delay implementation of what has been done to date. Early results, reported in NUREG-0744, Appendix D, Figure 6, pg D-12, showed that the temperature-dependence of the tearing modulus is small for the irradiated steels of low toughness, which are at issue here. It has been known for many years that the toughness of RPV steels in the upper shelf region of temperatures is limited. Although there has been no satisfying experimental method available to measure the upper shelf toughness, it has been the practice to simply truncate curves of toughness as a function of temperature. More than merely following the lead suggested by the plateau in the Charpy V-notch impact energy curve, the idea draws support from the thesis that high temperature (upper shelf) fracture resistance is as much . function of the tensile strength as it is of toughness. Because strength is a weakly decreasing function of temperature in this regime, no large effects would be expected with respect to the fracture strength. The staff therefore concludes that the limited available information on the temperature-dependence of the elastic-plastic fracture mechanics parameters is no impediment to the implementation of the recommended procedure. At the same time, further work along both the analytical and experimental lines is encouraged by the NRC, and in editing NUREG-0744 a diligent search was made to find and revise all elements of discouragement.

Comment 9a: (General) The most impressive part of the report (Appendix D) is the correlations between Charpy energy, flow stress, and J-resistance curves. J-resistance curve data from the literature have been taken and the fit found to be excellent, based upon several different materials and Charpy energy levels. The development of this simple but important correlation is commendable.

Resolution: The compliment is gratefully acknowledged. The subjective assessment as to the correlations being the most impressive is best left to the reader. It is pleasing to hear that others have observed similar results.

Comment 9b: (General) One of the most important contributions of this report is the correlations developed with Charpy shelf energy. This is a very useful correlation, but its limitations have not been clearly described. It is an understanding that the correlations were developed for only very small crack extensions, of the order that might be obtained from J_{Ic} tests. It is not clear whether, or at what level of crack extension, the correlation breaks down.

Resolution: Assuming that the specific correlation the commentor had in mind was that between the value of the J-integral at the intersection of the materials $J = f(T)$ curve with the loading line of slope $J/T = 50$ in.-lb/in.^{2} and the Charpy V-notch impact test upper shelf energy as shown on Figure 5.13, pg 5-22, in NUREG-0744, the stated understanding seems to be a misunderstanding. The actual intersections of the materials curves with the arbitrary load line can be seen in Figure 12, pg D-21, Appendix D, NUREG-0744. The materials curves were developed by Loss and his coworkers at the U.S. Naval Research Laboratory from their own J-R curves. The largest value of T for any given curve is that obtained at the value of J_{Ic} determined in accordance with the ASTM method for that parameter. The intersections all occur at much smaller values of T; i.e., large values of J (not necessarily the end of the J-R curve but as far as a power-law curve fit would go) and large values of crack extension. Thus the reality of the situation is diametrically opposite to the commentor's understanding. As to the limit of applicability, it was noted in the response to Comment 6a that the value of ω for cracks of interest in an RPV steel is the order of 10 or more on the J/T = 50 in.-lb/in.^{2} load line. The material J = f(T) curves for tough (large C_v USE) steels may only intersect the arbitrary load line at relatively low values of ω, but because the interest in Task A-11 is focused on steels with USE $\leq$ 50 ft-lb or less, the fact that the correlation gets fuzzy at high toughness is of little concern. The error in the lower bound curve of Figure 5.13, NUREG-0744, over the range $0 \leq C_v \leq 50$ ft-lb is quite small for the currently available data, although as more data become available, the error should be given critical reviews. The staff is confident that the licensee who has no J-R data for his RPV steels nor any means of obtaining such data can use the curve of Figure 5.13 as the source of data for a vessel safety analysis.

Comment 9c: (General) Pg 5-21 and Figure 5.13: NRL should be credited for the original concept of a C_v-J correlation. Note that the correlation applies only at 200°C and that the R curve varies inversely with temperature. A more up-to-date correlation is Figure 11 of the ASTM Philadelphia paper by Loss or Figure 14 of the NRC information meeting.

Comment 9d: (General) In Appendix B it is particularly clear that the NRL is not given credit for the C_v correlation with J. In fact the document gives the opposite impression.

Comment 9e: (General) Pg B-40: The idea to plot J_{50} vs. C_v was an NRL idea and should be so credited.

Resolution: Comments 9c, 9d, and 9e all focus on the same issue and will be answered collectively.

The question of who can claim first disclosure of an observed relationship such as the toughness/Charpy energy correlation presented in NUREG-0744 has no bearing on the proposal to implement the analytical procedure, nor can a regulatory publication be the forum for such a debate. The references mentioned do contain material that adds to the acceptability of the procedure being proposed; thus it is fitting to call attention to them. They are

(1) F. J. Loss, B. H. Menke, A. L. Hiser, and H. E. Watson, "J-R Curve Characterization of Irradiated, Low Shelf Nuclear Vessel Steels," ASTM Second International Symposium on Elastic-Plastic Fracture Mechanics, Philadelphia, PA, October 6-7, 1981.

(2) F. J. Loss, "J-R Curve Characterization of Irradiated, Low-Upper Shelf Welds," 9th Water Reactor Safety Research Information Meeting, National Bureau of Standards, Gaithersburg, MD, October 26-30, 1981.

In all fairness, credit must be given to researchers at the U.S. Steel Research Laboratory, Monroeville, PA, for the early correlations between toughness and Charpy energy. Broek (1974) cites the work of Barsom and Rolfe (1970) and illustrates it with a graph of K_{Ic}^2/E as a function of the Charpy impact energy measured at the same temperature as the plane strain fracture toughness. Note that $K_{Ic}^2/E = J_{Ic}$ for very small crack tip plastic zone sizes. Later work at U.S. Steel by Rolfe and Novak resulted in a correlation between toughness and the Charpy USE. History seems to say that the correlation at issue was only a modification of what already existed. To avoid the implication that it was unique with the advent of NUREG-0744, however, the text has been edited to call attention to the fact that Chapter 5 was based entirely on the NUREG appendices.

Comment 9f: (General)

Pg D-21: With respect to Figure 12, the data shown here indicate that a correlation between J_{Ic} and upper shelf energy could also be developed similar to that shown on Figure 14 with respect to J/T = 50. Such a correlation would be useful because of the remark contained in Section 6 that crack initiation must be assumed if the J_{Ic} value is to be exceeded. Curves in the report are not accurate enough to generate this curve accurately. However, using the available data from Figure 12, we obtained an equation that J_{Ic} in units of in.-lb/in.^{2} is equal to 6.13 times the upper shelf energy in ft-lbs. This straight line goes through all three points identified on Figure 12 that have a USE of less than 100 joules, through one of the points that has a USE of 108 joules, and is below all other points with the exception of one that has a USE of 159 joules. The coefficient is 0.794 for the SI units. If these results are used to calculate K_{Ic} as a function of the upper shelf energy, the following values are obtained:

USE:	30	40	50	60
K_{Ic}:	73	84	94	103

These values are in general agreement with some recent experimental data; however, the correlation used tends to become conservative at upper shelf energies in excess of 75 ft-lbs. The correlation used gives about 60% of the K_{Ic} that one would calculate from the Sailers-Corten upper shelf correlation between toughness and CVN.

Resolution: The analysis is most welcome. The Rolfe-Novak correlation (1970) mentioned above is

$$(K_{Ic}/\sigma_{ys})^2 = (5/\sigma_{ys})(C_v - \sigma_{ys}/20)$$

All the data used by Rolfe and Novak to establish their correlation were obtained at a test temperature of 80°F.

The Sailers-Corten correlation has been shown to be rather close to the Rolfe-Novak equation. Using the extrema from the tabulated values in a rearranged form of the equation

$$1 + 4\,(73/\sigma_{ys})^2 = (20)(30)/\sigma_{ys}$$

$$1 + 4\,(103/\sigma_{ys})^2 = (20)(60)/\sigma_{ys}$$

Further manipulation

$$\sigma_{ys}{}^2 = 600\,\sigma_{ys} - 4(73)^2$$

$$\sigma_{ys}{}^2 = 1200\,\sigma_{ys} - 4\,(103)^2$$

Solving for σ_{ys} results in a value of 35 ksi. Because the result is too small (58% of the minimum, nonirradiated yield of 60 ksi; a smaller fraction if radiation hardening is added), the commentor's conclusion seems to be correct; that is, the Charpy USE may correlate with more than one arbitrary elastic-plastic fracture mechanics parameter, but the correlations previously established for LEFM parameters seem to be distinctly different.

Comment 10: (General) Throughout the report the limitations of the method are not clearly delineated, and the assumptions made in developing various aspects are not listed. An example of this is the correlation between the J-T curves and upper shelf Charpy energy, based on fitting the J-Δa curve with a power law. This is applicable only for small crack extensions, but this limitation is not noted.

Resolution: Detailed treatment of the several limitations to the recommended procedure will not be found in the basic NUREG report (what is now Part I of NUREG-0744). The reason for this is that the basic report is a condensation of the appendices which, collectively, provide the technical basis for the resolution of Task A-11. If the commentor is displeased with the level of technical detail in the appendices, the problem cannot be resolved by the NRC staff. The appendices have been treated, by and large, as contractor's reports, at least to the extent that the authors, as members of the Task A-11 Technical Team, were professionally responsible for the content of what they prepared. It should be noted, however, that the specific example cited is not well chosen. The C_v USE correlation with J_{50} need not rely on the power law fit to J-R curves. It will emerge equally well if the materials J-T curves are developed by taking the actual J-R curve slope and reducing it to a value of T. As a matter of fact, that might be preferred since it is true that one power law relationship cannot fit the entire J-R curve.

Comment 11: (General) The NUREG states that the USE at end of life (EOL) should be established in accordance with "10 CFR 50 and the ASME Code." Neither of the referenced documents provides this information. Regulatory Guide 1.99 provides some guidance.

Resolution: The contention that the comment focused on merits some explanation. The correspondent has called attention to the second sentence in the second paragraph on pg 6-1 of NUREG-0744. This is another situation where the staff is obliged to restate a principle which guides NRC actions: the staff will establish standards of safety and require that the industry meet them, but the means or methods by which the standards are met is the responsibility of the industry. The same thing was said in response to Comments 2a to 2e. Applying the principle here, the licensees must show that their RPVs exhibit adequate toughness. Guidance available to them in the Federal and ASME Codes says that the NRC will accept values of Charpy V-notch impact USE of 50 ft-lb or more as evidence of adequate toughness. In calculating current or EOL USE, licensees may use Regulatory Guide 1.99 with the assurance that the NRC staff will approve of the method. Other methods can be used, but the staff will be obliged to conduct a thorough evaluation of any such submittals to ensure that the result does, indeed, meet its standards of adequate safety. Therefore, neither 10 CFR 50 nor the parts of the ASME Code it references can be expected to provide the calculational details to what is a plant-specific engineering problem. The text of NUREG-0744 on pg 2-2 and 2-3 carries some carefully chosen words about toughness and Regulatory Guide 1.99.

Comment 12a: (General) Acceptance criteria for fracture evaluations, based on J-integral analyses are even less mature than the analysis methods. ASME Code Section XI has adopted criteria based on crack initiation for

Level A and B loading conditions and on crack arrest for Level C and D loading conditions. Because the low upper shelf issue concerns only some operating reactors, none of which are Combustion-Engineering reactors, criteria similar to Section XI rather than Section III appear more appropriate for safety evaluations. (Criteria similar to Section III Appendix G should be used only to generate P-T curves.)

The development of acceptance criteria should be encouraged by the NUREG by specifying that evaluations are to be judged on a plant-by-plant basis.

Comment 12b: (General) Pg 6-1, Lines 20 and 21: Appendix G requires that the stress intensity factor due to pressure be less than half K_{IR} (ignoring thermal stresses). The K_{IR} curve is very conservative compared to actual vessel failure, indicating a margin against failure considerably greater than the nominal factor of 2. Figure 5.8 illustrates that when the toughness is at upper shelf levels, the design pressure limit is adequate protection against failure, even with the presence of large flaws. Because the tests (in Figure 5.8) were not on low USE material, however, they provide little guidance to the establishment of safety margin requirements. In fact, there does not seem to be an adequate basis for selecting criteria at the present time.

This is especially apparent if transient loadings and temperature dependent properties are considered (see Comment 12a).

Comment 12c: (General) It is difficult to relate reactor vessel safety to a J/T plot. Factors of safety, in terms of load, are not obvious using this type of plot. It appears that several conservative assumptions have been made in order to simplify the generic type of analysis presented in the report.

For material toughness (J-resistance curves) with large "T" moduli, the analysis appears to show adequate margins of safety. However, for low T moduli, the method of J/T analysis might give a premature indication of problems in existing nuclear reactors due to the excess conservatisms of the generic approach. Specifically, the separation of geometry and material effects given in the report is not so obvious and seems to be based on an assumption of an infinite center-cracked plate. Work under EPRI contract RP1237-2 has shown that the infinite center-cracked plate assumptions are indeed conservative. However, they may be overly conservative for purposes of establishing adequate safety margins in an ASME Code context.

Comment 12d: (General) The acceptability criteria, given on pg 6-1, must be more clearly specified. The criteria for emergency faulted conditions appear to have been developed to be consistent with Section XI, but are misstated. It appears that the criterion should be $J_{appl} < \frac{1}{2} J_{50}$. For normal, upset, and test conditions, the criteria are not spelled out, and the reader is left with only the guidance to use a margin equivalent to Appendix G. Most readers are not prepared to derive such a criterion.

If the criteria are intended to be as stated above, they appear to be very conservative, and may not be much improvement over the LEFM calculation results. This can be shown by example calculations.

The most important omission in the criteria is the reference flaw size to be used in the calculations. The J/T = 50 line suggests an assumed flaw depth of 0.25 inches.

A number of J-T curves are provided in the report, but no guidance is given as to which one to use.

Unless more specific recommendations are made on these points, the methodology will not be applied uniformly.

Comment 12e: (General) Section 6, Licensing Aspects, is intended to describe the "approach and methodology" acceptable to meet the requirements of 10 CFR 50, Appendix G, when the 50 ft-lb USE requirement cannot be satisfied. The following comments are in the sequence in which they apply to Section 6:

- o Absent plant-unique property curves, "the lower bound J (at J/T = 50) = f(USE) curve" is to be used. Which of the two different curves in the NUREG is preferred (Figure 5.13 or Figure 14 on pg D-23)?

- o The USE at end of life (EOL) should be established in accordance with "10 CFR 50 and the ASME Code." Neither of the referenced documents provides this information. Regulatory Guide 1.99 provides some guidance.

- o If the EOL USE < 50 ft-lb, one option is to perform an analysis "in accordance with the concepts and examples in the Appendices to this report." The appendices are an informal collection of papers prepared by various individuals. They contain a number of concepts and examples and the contents of one appendix may conflict with the contents of another. There is no way to tell which, if any, method is preferred. It is doubtful that some combinations of selected concepts would, or should, be acceptable.

- o The third paragraph "recommends" certain safety factors to be used with EPFM analyses:

  (a) "(F)ailure conditions must be calculated conservatively from the J and T values at the intersection of the relevant [$J = f(T)_{mat}$] curve and the loading line of slope: J/T = 50. (The format of the bracketed quantity has been corrected in preparing this quotation.) Use of the this value of J, termed J_{50} in the report, is meaningful but "very conservative" (emphasis not added), as stated in the third line of pg 5-22. The T value at this intersection is meaningless in application, because the J/T-applied slope is approximately an order of magnitude higher than 50.

(b) "For normal and upset conditions (Levels A and B), the margin between failure and operating conditions must be equivalent to the margin now required by Appendix G." While the intent is meaningful, only individuals really knowledgeable in the subject can interpret it. It would be much clearer to state that four times the J-applied should be less than J_{50}.

(c) "For emergency and faulted conditions (Levels C and D), the value of T_{appl} must be no more than one-half the value of T at the above named curve intersection." This criterion is very unconservative. It implies that a J_{appl} value of the order of 5 J_{50} is acceptable. This is clearly an error, as force equilibrium would not exist. We suspect that the intent was to restrict J_{appl} to $J_{50}/2$, since this would be consistent with the factor of safety on K imposed by Section XI, IWB-3600.

(d) "The evaluation must recognize flaw growth at $J > J_{Ic}$." Although this a true statement, no further guidance is given. The data used in the report could be used to develop an acceptable lowerbound J_{Ic} vs. C_v curve in the same manner as the J_{50} curve was developed. (Scaling the figure in Appendix D, approximately $J_{Ic} = 6\ C_v$.) Specific statements should be included providing criteria for crack initiation as has been done for crack instability. The situation is almost identical to that in Section III in determining the applicability of Levels A and B, on one hand, and Levels C and D, on the other. In addition, such a J_{Ic} relationship provides information as to the upper limit of validity of the Code fracture toughness curves which must be established in order to determine the type, LEFM or EPFM, of analysis which is appropriate to specific cases.

- "(T)he need to modify Appendix G to 10 CFR 50 (and possibly, Appendix H) will be established." On the basis of the developments presented in the NUREG, these appendices should be revised prior to final issuance of the NUREG.

- The discussion starting with the last paragraph on pg 6-2 is similar to that on pg J-13, but with changes which make the two inconsistent. This an example of the situation described by the third item above.

The text of the report defines the regulatory and material aspects, and the problem describes the solution, concluding with sections titled "Licensing Aspects" and "Ancillary Aspects." Although the attachment contains many detailed comments with the objective of improving the report, the most important problem with the NUREG is in Section 6.

Comment 12f: (General) A very specific procedure must be defined and specific criteria stated if the NUREG is to "define reactor vessel safety criteria." This would best be accomplished by replacing the present Section 6 of the NUREG text with a presentation of the following:

- o Computation of J-applied, including determination of the stress correction factor and geometry factor for surface, subsurface, and through-wall cracks. (Note that there is no need to include T or J/T methods here.)
- o Acceptable lower-bound values of J_{Ic} and J_{50} as a function of CVN USE. Permission to use plant-unique experimental values.
- o A statement that the Section III and XI toughness curves are not applicable above $K_{Ic} = [EJ_{Ic}]^{\frac{1}{2}}$.
- o Specific factors of safety to be applied to J_{Ic} and J_{50} for specific situations. For example, these could be

(a) $J_{appl} \leq J_{50}$	when evaluating leak-below-break with a = t
(b) $2\ J_{appl} \leq J_{50}$	when evaluating instability of a postulated defect, defined as described in Appendix J of the NUREG draft, for Emergency and Faulted Conditions.
(c) $4\ J_{appl} \leq J_{50}$	when evaluating instability of the defect defined by Appendix G, a/t = 1/4, a/ℓ = 1/6 flaw for Normal and Upset Conditions.
(d) $2\ J_{appl} \leq J_{Ic}$	when evaluating initiation under Emergency and Faulted Conditions of the postulated defect described in Appendix J of the NUREG draft.
(e) $10\ J_{appl} \leq J_{Ic}$	when evaluating initiation under Normal and Upset Conditions of an inservice determined indication.

- Specific acceptance criteria, perhaps conditional on the operational response. For example, this could be satisfaction of a, b, c, or d below:

  (a) Acceptance without operational restriction when leak-before-break conditions, see D.4.a, can be satisfied for all expected or postulated conditions of internal pressure.

  (b) Acceptance without operational restriction when all of the following conditions are met:

    (1) Appendix G of Section III or the instability conditions of D.4.c, depending upon the metal temperature, for Normal and Upset Operating Conditions.

    (2) IWB-3600 of Section XI or the initiation condition of D.4.e, depending upon the metal temperature, for Normal and Upset Operating Conditions.

    (3) Initiation condition of D.4.d for a postulated defect subject to Emergency and Faulted Conditions.

  (c) Acceptance with a requirement for shutdown within 24 hours and requalification of the RPV if the criteria of D.5.b(1) and (2) are satisfied and the instability condition of D.4.6 is satisfied, but D.5.b(3) is not satisfied.

  (d) The RPV shall be annealed in a manner acceptable to the Commission.

Resolution: Comments 12a through 12f deal with the same general subject. In one way or another, each addresses the issue of safety margins. For the most part, the focus is on Chapter 6 of NUREG-0744. If the reader compares Part I of this document (NUREG-0744, Revision 1) with the "For Comment" version, the reader will see that significant changes have been made. Those changes resulted from efforts to address Comments 12a through 12f.

To explain Chapter 6 and the nature of the current revision, one should start on pg A-2 of NUREG-0744. The first three (by far the most important) Sub-Tasks of TAP A-11 were: (a) identify the relevant material parameters, (b) develop a structural analysis, and (c) define RPV safety criteria. Sub-Tasks A and B were to define the experimental and analytical bases for calculating RPV failure conditions when the amount of plastic deformation made LEFM inapplicable. Given the ability to predict failure, it seemed to be a straightforward next step to establish a margin (or margins) to prevent failure. During the course of Task A-11, it became evident that the margin of safety should be a function of the operating conditions: that is, large for normal and upset conditions, corresponding to Levels A and B of the ASME Code, but less for Level C, emergency, and smaller still for Level D, faulted. Chapter 6 was an attempt by the Task Manager to provide the safety margin and complete Sub-Task C. It was not given to the A-11 Technical Team because the job involved establishing a regulatory position, which is the responsibility of the NRC staff, not its consultants.

The comments received relative to Chapter 6 demanded extensive rework (sometimes called an agonizing reappraisal). One strategy would have been to work the rather extensive analysis of Comments 12e and 12f into Chapter 6. Doing that would result either in the act of issuing the NUREG under conditions where one of the most important parts of the regulatory action, the safety limit(s), would go into force without the benefit of public review and comment or in the tedious, time-consuming action of going through a second "For Comment" version. A second strategy, the one that the NRC management selected, was to delete Sub-Task C, contingent on the ASME Code Committee accepting the task of defining new safety limits. In response to a formal request from the A-11 Task Manager, the Working Group on Flaw Evaluation of the Sub-Group on Evaluation Standards, Section XI, ASME Boiler and Pressure Vessel Code Committee has agreed to consider the task. When the Committee's work is complete and a report issued, the NRC will review the results and, if the staff approves, adopt the results by reference.

Because of the above revision in the Task Plan, there is little need for detailed replies to the several comments. A few points do, however, deserve at least passing mention.

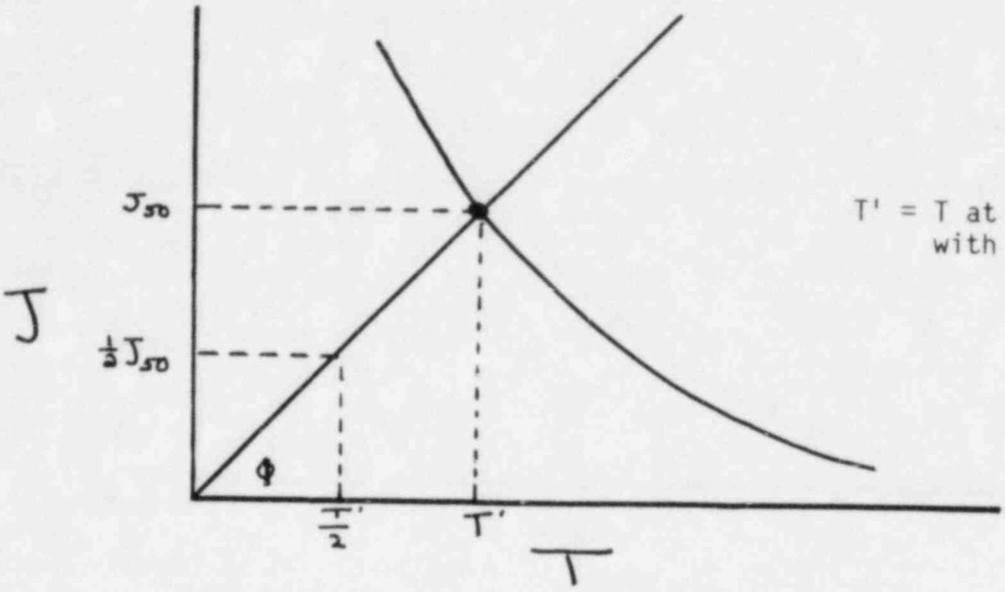
The supposition in Comment 12c that the separation of stress and geometry terms was based on infinite center-cracked plate assumption(s) is incorrect. Reference should be made to Appendix B, pg B-12, ff, the section titled "Analysis of J Versus T Applied Curves for Through Cracks in Pressure Vessel Walls." The separation of the two factors (stress and geometry brackets) was accomplished in that section. A physical justification for the separation can be provided by the argument that follows. There are four permutations of the extrema of stress and crack size: small stress and small crack; large stress and large crack; small stress and large crack; and large stress and small crack. Considering them in the same order: small stress and small crack cannot be an elastic-plastic failure problem; large stress and large crack need not be considered because it is physically impossible, a large crack (large through-thickness crack) cannot be pressurized enough to build up large stresses because it will open up and release the pressurizing fluid; the cases of large stress and small crack (a part-through crack) and small stress and large crack (through-wall crack) can be realized and are the configurations analyzed in Appendix B. However, because they are the only cases of importance to the RPV beltline region analysis, cross-terms between the stress and geometry factors can be neglected and the separation shown in Appendix B, Equation (21), can be established without recourse to crack plate analyses.

Comment 12d requires a few responses. First, it is not clear what the commentor meant by "not much improvement over the LEFM calculation results." If a large relaxation in allowable pressure from an elastic-plastic analysis was expected, no sympathy for the dashed hopes can be mustered. Not unexpectedly, application of the recommended procedure to an RPV with an assumed USE of 50 ft-lb and a design flaw of one-fourth of the wall results in a margin (difference between calculated fracture and normal operating pressures) of two to three. Second, there is no flaw size assumed in the loading line of J/T = 50 in.-lb/in^2. The origin of that slope, being about one-tenth the loading lines of RPVs with flaws of interest, was discussed before (see Resolution of Comment 6a). Third, none of the J = f(T) curves given in NUREG-0744 are recommended for use in plant-specific RPV calculations. If J-R curves for a specific vessel are available or can be obtained, they should be used. If curves reported by a licensee, however, are at wide variance with data judged reliable and accurate by the NRC staff, the results may be called into question. In the absence of J-R curves, the lower bound correlation curve between Charpy USE and J_{50} is recommended.

The third paragraph (second bullet) in Comment 12e is identical to Comment 11. The reader's attention is directed to the resolution of that comment.

The statement in Comment 12e that "(t)he T value at this intersection is meaningless in application because the J/T-applied slope is approximately an order of magnitude higher than 50" is not a valid criticism. The conservatism is properly identified and was so noted in the response to Comment 6a. An over-riding consideration is the equally if not more important fact that the value of ω on both the conservative loading line and on the materials curves where they intersect the loading line is high enough the bring the resulting analysis within the bounds of "valid" elastic-plastic theory (also discussed under Comment 6a Resolution).

The paragraph concerned with the criterion suggested for Levels C and D is not clear to the NUREG author. Elementary geometry leads to the conclusion that one-half of T results in one-half of J, as well (see accompanying sketch).



Because $\tan \phi = J_{50} / T' = xJ_{50} / \frac{T'}{2}$, $x = \frac{1}{2}$,

and the source of $5\ J_{50}$ is a mystery to the NUREG author.

The basis for the statement in the next paragraph that $J_{Ic} = 6\ C_v$ by scaling a figure in Appendix D also remains a mystery but does not seem important enough to resolve. With respect to the idea that there will be some crack extension (if, indeed, a crack actually exists) for $J > J_{Ic}$, perhaps it is more important to look at the other side of the coin. If an analysis of an RPV shows that $J < J_{Ic}$ for accident loads, further analysis (elastic-plastic or otherwise) is unnecessary.

It has been determined that neither Appendix G nor Appendix H of 10 CFR 50 require any modification as a result of the resolution of Task A-11.

The paragraph in Comment 12e that cites the last paragraph on pg 6-2 of the NUREG as being inconsistent with the paragraph from which it was taken (on pg J-13) does not seem valid. If the two paragraphs were any more identical, the former would be a quotation.

The Comment 12e is recommended to the Section XI, ASME Code Committee for consideration in the task of establishing safety limits.

Comment 13: (Specific) Pg 1-1, Lines 8 and 9: This sentence should read: "Using this method, an assessment could be made to determine if certain older vessels still have adequate toughness."

Resolution: The recommended rewrite does not exactly present the message the author had in mind. However the text has been changed to what the staff believes to be a fair compromise.

Comment 14: (Specific) Pg 1-1, Line 9: Following the words "marginal toughness," the words "according to the current, simple assessment method" should be restored.

Resolution: The essence of the comment was incorporated into the edited NUREG text.

Comment 15: (Specific) Pg 1-1, last paragraph, Line 2: Change word "will" to "may."

Resolution: Agreed; the change was made.

Comment 16: (Specific) Pg 1-2, Line 3: The proposed safety analysis is to be performed only if C_v USE < 50 ft-lb, but the NUREG stated that LEFM would be inapplicable if C_v USE $\geq$ 50 ft-lb. This seems to imply that a plastic analysis is justified for C_v USE < 50 ft-lb, and yet, no defense is made for this methodology. If this is what is intended, that a plastic analysis is justified for C_v USE < 50 ft-lb, some information must be added.

Resolution: The staff holds the opinion that the comment was made because the idea in the statement cited was taken out of context or too broad a meaning was ascribed to it. The statement in the NUREG was made for the express purpose of pointing out that the fracture resistance of a steel with Charpy USE equal to 50 ft-lb, within the range of upper shelf temperatures, is so large that the amount of prefracture crack-tip plastic deformation will be so great as to violate the LEFM boundary condition of a relatively small plastic zone size. The discussion of this aspect under Comment 3 (in which Comment 32b was cited) applies here as well.

Comment 17: (Specific) Pg 1-2 and 3-6: The statement that LEFM is not applicable to the problem treated in this report is incorrect. It will produce conservative estimates of critical flaw size. The proposed method should enable more margin to be demonstrated.

Resolution: Applicable rebuttal discussions are given under Comments 3, 16, and 32b. Of course, LEFM methods <u>can</u> be <u>applied</u> to the problem of ductile failure, but the fact that the results will, in general, be wrong (that is, there will be a significant error between observation and calculations) justifies the use of the word "inapplicable."

Comment 18: (Specific) The upper shelf energy can be decreased by irradiation, but this will not happen for all materials. Also, the paragraph on size effects in toughness testing is not clear.

Resolution: Comment 18 followed 17 in the original submittal; as such, the first sentence refers to pg 1-2 in NUREG-0744. In reply, diminution of the Charpy V-notch USE is generally expected in RPV steels as a result of neutron radiation of operating temperatures. Exceptions, which are rare, are irrelevant, particularly with respect to the point which was being made in the part of the NUREG text cited.

The second sentence presumably refers to the second paragraph on pg 2-1. Because the part of the NUREG text cited was written as background information with no ambition of being the authoritative textbook on the subject, the staff feels that no changes are necessary.

Comment 19: (Specific) Pg 2-1: The ductile-brittle transition temperature is often synonymous with NDT; the latter does not shift with specimen size.

Resolution: The NUREG does not state that NDT is a function of specimen size; thus no modification is needed.

Comment 20a: (Specific) Pg 2-1: C_v energy is a poor indication of the transition region in general. NRL research shows a large variability in C_v in energy at the NDT. C_v 50 or 35 ft-lb indices are used to indicate the shift in transition region and never were used to indicate the region itself.

Resolution: The comment dwells on a minor technical point not really germane to the main thrust of the safety issue. It is true that specific values of Charpy impact energy stand up better to the idea of being barometers (the commentor said "indices") of the transition temperature than indicators of the region itself. Editorial changes have been made to reflect this idea. However, reading of the final paragraph of article 14-2 on pg 375 of Mechanical Metallurgy by George E. Dieter, Jr. will serve to support the idea of using an energy measure. Dieter wrote: "The energy transition for V-notch Charpy specimens is frequently set at a level of 10 or 15 ft-lb." The nuclear industry, being more prudent than ship builders, has chosen the 35 ft-lb level.

Comment 20b: (Specific) Pg 2-1, Lines 19 and 20: The 50 ft-lb C_v energy level does not define the transition temperature, but has been used as a supplementary index per ASME Appendix G. This statement should be corrected.

Resolution: The above discussion applies. The part of the NUREG text cited makes no reference to Code definitions; it is merely an historically correct general description. No editorial changes beyond that noted above are called for.

Comment 20c: (Specific) Pg 2-1, third paragraph: The transition temperature has not been defined at a 50 ft-lb level by anyone, certainly not by the Code.

Resolution: The idea that 50 ft-lb is not used to indicate the ductile-to-brittle transition in RPV technology is essentially correct, so that value was deleted.

Comment 21: (Specific) Pg 2-1: Text should be revised to say: "Neutron radiation from an operating reactor core may embrittle the sensitive RPV steel."

Resolution: The suggested change is unwarranted. The sentence reads: "Neutron radiation from an operating reactor core will embrittle the RPV steel." The steel need not be sensitive; the relative sensitivity only establishes the rate and, perhaps, the level at saturation, if such actually occurs. Embrittle only means a loss in toughness, and that will generally occur.

Comment 22: (Specific) Pg 2-1: Text should say: "...the transition temperature regime is increased."

Resolution: The suggested edit was adopted, although it seems to be a fine technical point.

Comment 23: (Specific) Pg 2-2, Lines 14 and 15: This should be corrected to read: "Because certain high copper welds have low USE levels...."

Resolution: Not all high-copper weld metals exhibited low USE levels, so an editorial change along the line suggested was made.

Comment 24: (Specific) Pg 2-2, second paragraph: The RT_{NDT} is not the specific transition temperature used. The RT_{NDT} is an index which is used to determine fracture toughness properties in subsequent calculations. The second sentence of this paragraph applies to 10 CFR 50, not to the ASME Code. This difference should be clarified because of the way the first sentence reads.

Resolution: The second paragraph on pg 2-2 of NUREG-0744 does not mention RT_{NDT}. This parameter is mentioned in the first paragraph, but the comment appears inappropriate there. Nowhere else on pg 2-2 and 2-3 is RT_{NDT} written, so no changes were deemed necessary.

Comment 25: (Specific) Pg 2-3, penultimate sentence: The word "unacceptable" is unacceptable. This is a measure of the compliance with regulations, not a measure of the compliance with need.

Resolution: An editorial change to a more specific statement was made.

Comment 26: (Specific) Pg 3-1, third line from the end: The word "generally" should start the parenthetical phrase "1/4 of the wall thickness." This 1/4th of the wall thickness is not a requirement.

Resolution: The comment is inappropriate. The use of the flaw depth equal to 1/4th of the wall refers to the work reported in Reference (8), not to any regulation.

Comment 27: (Specific) Pg 3-2, Line 9: The first three words should be changed to "behavior in the transition toughness region."

Resolution: The editorial change suggested was made except that the word "temperature" was not changed to "toughness."

Comment 28: (Specific) Pg 3-2: Text should say: "...material must develop 50 ft-lb of Charpy energy at a temperature no higher than 60°F above the RT_{NDT}."

Resolution: Extensive editing of the NUREG eliminated the sentence cited.

Comment 29: (Specific) Pg 3-2, Lines 10 and 14: ASME Appendix G never addressed upper shelf. The C_v test is used to ensure that the material possess a reasonably well-defined transition; i.e., that its behavior is typical for materials of its type.

Resolution: Extensive editing of the NUREG eliminated the cited sections.

Comment 30: (Specific) Pg 3-2, first full paragraph: There is no requirement in the Code that the materials must develop 50 ft-lbs of Charpy energy at a temperature no higher than 60°F above the NDT.

Resolution: Extensive edition of the NUREG eliminated the sentence cited.

Comment 31a: (Specific) Pg 3-3, Line 7: The technical basis for some of the 10 CFR 50 requirements is found in Reference 8 of NUREG-0744. The USE requirements of concern in this NUREG are not mentioned in Reference 8.

Resolution: The comment is correct. At the same time, the NUREG section cited does not say (or imply) that the upper shelf energy degredation problem or the related requirements can be found in Reference 8; thus no change is needed.

Comment 31b: (Specific) Pg 3-3, first full paragraph, third line: This sentence is not correct. The technical basis for the NRC requirements cannot be found in Reference 8.

Resolution: The comment refers to the same statement as Comment 31a but in much stronger language. The staff does not agree that it is incorrect to cite Reference 8 as a technical source of the basis for the NRC RPV requirements. Assuming the commentor has taken issue with the choice of words and standing by the original statement, the author believes no changes need be made.

Comment 32a: (Specific) A point which needs clarification relates to introductory comments implying the "adequacy" of RPV steels whose C_v USE is above 50 ft-lb. Certainly this may be true, but the report has not referenced a factual basis for such a statement. The problem appears to be associating this conclusion with a generic K_{IR} (initiation) curve that supposedly reaches 200 ksi $\sqrt{in.}$ There is no evidence to support the latter because upper shelf K_{Ic} numbers do not exist for steels with USE greater than 50 ft-lb. Using K_{Jc} values obtained from J_{Ic}, it can be reported that the HSST welds with C_v USE of 74, 76, and 80 ft-lb have approximate K_{Jc} values at 200°C of 170, 132, and 119 ksi $\sqrt{in.}$, respectively. In other words, the concept of a high initiation toughness associated with RPV steels with USE greater than 50 ft-lb is baloney.

Comment 32b: (Specific) Pg 3-4: The commentor questions the inference that $K_{Ic} \leq 200$ ksi $\sqrt{in.}$ for steels having C_v > 50 ft-lb because

- o No valid K_{Ic} values have ever been measured at the upper shelf for RPV steels.
- o NRL data on HSST welds (which have C_v USE > 50 ft-lb) give K_{Jc} values < 200 ksi $\sqrt{in.}$ It is true that $K_{Jc} \neq K_{Ic}$ on the upper shelf but the discussion is confusing.
- o What is the basis for the statement that if 10 CFR 50 is met (i.e., C_v USE > 50 ft-lb) there is no concern? The initiation toughness on the upper shelf is not known.

Comment 32c: (Specific Pg 3-4: Here the NUREG implies that for the low USE material 200 ksi $\sqrt{in.}$ is guaranteed at RT_{NDT} + 200°F. This is not an obvious fact and no defense is made for this information. If 200 ksi $\sqrt{in.}$ is not guaranteed, LEFM may not be conservative for normal and upset condition evaluations, and an alternate analysis may be required.

Resolution: All three comments point to the same perceived weakness; that is, there is an inadequate technical basis to assert that RPV steels with Charpy V-notch impact USE levels of 50 ft-lb or more actually have enough fracture resistance to provide as wide a margin of safety as the NRC, the nuclear industry, and the public desire. Even if recent, reliable fracture test results show that 50 ft-lb is more likely related to a toughness of less than 200 ksi $(in.)^{\frac{1}{2}}$, other conservatisms that can be noted in Reference 8 are enough to provide an overall safe margin. The data cited, however, are welcome and hopefully will serve as motivation for more upper shelf toughness testing to firm up the position. The criticisms in the above three comments seem unduly harsh because the portion of the NUREG cited merely restates in somewhat abbreviated form a more detailed discussion given in Appendix J. Also, the entire idea was to present an illustrative example and certainly not to imply that a toughness of 200 ksi $(in.)^{\frac{1}{2}}$ can be assured, much less guaranteed for USEs of 50 ft-lb and/or at upper shelf temperatures.

The statement that valid K_{Ic} measurements never have been made at upper shelf temperatures on RPV steels is important and must be emphasized, especially because it bears on several other comments.

The staff, while sympathetic to some of the concerns of the commentors, sees no reason to alter the text because of the mainly historical and tutorial goal of the sections cited.

Comment 33: (Specific) Pg 3-3: Operation of a plant after a faulted condition may not be possible without careful inspection and checking, but it is inappropriate to say it will not be possible.

Resolution: The portion of the NUREG cited does not assert that it will not be possible to operate a plant after a faulted condition accident (ASME Level D). It does say: "it must be assumed that operation after a faulted condition is not possible." To assume that it is not possible is very different from saying it is not possible; thus no change in the text is necessary.

Comment 34: (Specific) Pg 3-4: Here $K_I < K_{IR}/3.2$; pg 3.1 (bottom) states a requirement of $K_I < K_{IR}/2$ (i.e., a factor of 2 on pressure is a factor of 2 on K_I). This is inconsistent.

Resolution: There is no inconsistency. The requirement that the pressure be multiplied by 2 in calculating K_I only means that the calculated stress intensity factor will be as much as a factor of 2 greater than the "real" K_I for the assumed flaw and RPV dimensions. Because the total K_I is a linear sum of twice the pressure-induced and thermal-stress-induced Ks (i.e.: $K_I = 2K_{IP} + K_{IT}$), the conservation will be 2 in the absence of thermal stresses. The other ratio relates to the margin required as a simple difference between the reference toughness, K_{IR}, and the above-calculated stress intensity factor, K_I. Originally (Reference 8 in NUREG-0744) that difference was to be a factor of 3. Later, the margin was given as a factor of 10 difference between the flaw sizes calculated as final (at fracture) and allowable (based on K_I). Still later, on reverting to the stress field parameter, the factor of 10 on flaw size became a factor of $\sqrt{10}$ on K, or, approximately

$$K_I \leqq K_{IR} \quad (3.2)$$

as shown on pg 3-4 of NUREG-0744. Because there is no inconsistency, no change in the text is necessary.

Comment 35: (Specific) Pg 3-4: The logic developed in the next-to-last paragraph seems to lead to the statement "priority should be given to normal and upset conditions."

Resolution: The message behind the statement "...priority must be given to rules applicable to emergency and faulted conditions..." may not have been clear. The idea was that because there are rules for normal and upset--but not for emergency and faulted--the latter were more important. The issue was (hopefully) resolved by adding the word "formulating" as the object of the giving of priority.

Comment 36: (Specific) Pg 3-5: The latest version of 10 CFR 50 does not require dynamic fracture toughness testing.

Resolution: The portion of the text cited is that which describes in abbreviated form the three requirements given in Article V.C of Appendix G, 10 CFR 50, which licensees must follow if the steel in an RPV exhibits a USE of 50 ft-lb or less. Specifically, the commentor referred to the statement in the NUREG that said that tests "such as dynamic fracture toughness" must be performed. True, 10 CFR 50 does not require them, but Article V.C.2. says, in part, "Additional evidence shall be obtained from results of supplemental tests, such as measurements of dynamic fracture toughness...." Therefore the comment can be set aside.

Comment 37: (Specific) Pg 3-5, Line 12: A thermal anneal may not be the only or best solution. This NUREG should not limit the possibilities to one specific solution.

Resolution The NUREG imposes no such limitation. The idea of a thermal anneal to recover toughness lost by neutron radiation is treated adequately in 10 CFR 50, and the portion of the text cited is an abbreviated statement of the Federal Code requirements. Therefore, no change is required.

Comment 38: (Specific) Pg 3-3 and 3-4, last two lines of 3-3: Much of this is based on Appendix J, but it is taken out of context and there needs to be some rewording, particularly in the lead-in sentences and in the words which follow the table on pg 3-4.

Resolution: Because it is taken from Appendix J where the discussion is more detailed (and, perhaps, more lucid), an editorial note directing the reader to the source has been added. Some of the differences in word choices were intentional attempts to keep that part of the NUREG from being a direct quote. With a more direct referral to Appendix J, the need for the suggested rewording hopefully disappears. Because specific editorial changes were not mentioned, further editing would only result in another portrait of Appendix J without impacting the basic thrust of the safety issue resolution.

Comment 39: (Specific) Pg 4-1: One of the requirements stated for an applicable method is that it has "direct physical significance with respect to safety margin determined." The J-T method as proposed violates that requirement because it is impossible to attach a physical meaning to the result obtained directly from the procedure without further calculation. The critical flaw size is lost, as is important information like the amount of stable crack growth predicted.

Resolution: The comment, as written, ignored a key element of the portion of the NUREG text cited (the last paragraph on pg 4-1). The paragraph enumerated the several attributes that, to varying degrees, the members of the A-11 Technical Team held as desirable in the resolution of the safety issue. Missing from the comment is the idea that "direct physical significance" (etc.) was an attribute that the solution should have. If the correspondent believes that it does not have the attribute, then, in his eyes, the author has failed in that respect. In no way were any of the attributes called "requirements," as was done in Comment 39. In rebuttal, there are others who believe that the form of the solution presentation does have physical significance, and the author is comforted by their encouragement. As those who are addressing the task of defining the safety margin do their work for Section XI of the ASME Code, the resulting familiarity with the method will dispell much of the anxiety expressed in the comment. No action is required at this time.

Comment 40: (Specific) Pg 4-1, second paragraph, third line: The word "considerable" may be misleading; suggest it be replaced by the word "significant." The last word in the sixth line should be "flow" not "flaw."

Resolution: Both edits are acceptable as improvements.

Comment 41a: (Specific) The phenomenon of a power-law R curve is a key element of the J vs. T analysis diagram. Remember that the R curves used to be bilinear before the NRL clarified things and a credit line should be inserted here (say, pg 5-1) to reflect this situation.

Comment 41b: (Specific) Pg 5-1: It is physically unreasonable to assume that J-R curves are continuously nonlinear. This implies an infinite value of J at a very large crack extension. In fact, the R curve must eventually become flat. In addition, the Naval Research Laboratory (NRL) should be credited for the formulation of the power-law R curve here.

Resolution: It is very difficult to respond to the comments. The nonlinearity of J-R curves has been a subject of active discussion for more than 6 years. Careful reading of Paris' first definitive paper on the tearing modulus (Reference 1 in NUREG-0744) should convince the knowledgeable reader that Paris held that the phenomenologically correct shape of curves of $J = f(\Delta a)$ would exhibit variable slope (i.e.: would be curves, not straight lines). He clearly said that straight-line graphs of J-resistance curves were a matter of convenience and ease of representation. The A-11 Task Manager, in 1979, reanalyzed the data in NUREG/CR-0859 and showed that they yielded curves of $J = f(\Delta a)$ which were linear on log-log coordinates, clearly implying a power-law function. That effort resulted from more than a year of discussion with several people around the country who were working in the field. The results of the analysis also were freely discussed (but not published), particularly with some members of the Task A-11 Technical Team. The conclusion which the author wishes to draw here is that acceptance of nonlinear J-resistance curves in the late 1970s was general because it was an idea whose time had come. The foundation was there in the form of nonlinear G-resistance curves with which we all are familiar (Broek, 1944). Deciding who has first claim to the disclosure of nonlinearity in J-R curves cannot be the responsibility of the A-11 Task Manager.

A word of caution is in order. As better experimental data have become available, especially from the Naval Research Laboratory, it has become apparent that a single power-law functional relationship cannot be written to fit J-R results closely over a large range of Δa. The importance of this assertion lies in the danger associated with the extrapolation of materials curves of $J = f(T)$ beyond the range of measurements in order to obtain a fast fracture estimate as an intersection with a loading curve.

Comment 42: (Specific) Pg 5-1, last paragraph, second line: Suggest that the words "ensuing fracture" be replaced by "ensuing crack." Such wording would be more accurate and less confusing to those who consider that a fracture means it's blown up.

Resolution: The recommended change was made, except that it seemed even more meaningful to say "ensuing crack extension."

Comment 43: (Specific) Pg 5-2: Figure 5.1 should show J_{Ic}.

Resolution: Showing J_{Ic} on Figure 5.1 is not germane to the purpose, which is to illustrate, schematically, the way to determine crack instability (the onset of test fracture) under conditions that can be analyzed with applied elastic-plastic fracture mechanics. In fact, it would be a distraction. Also, the current methods for determining J_{Ic} involve data reduction to obtain two straight lines--the blunting line and a linear representation of the J-R curve between the exclusion lines. The schematic material curve of Figure 5.1 would not accommodate the two straight lines and, at the same time, serve the author's purpose, which is to emphasize the nonlinearity of the J-R curves.

Comment 44: (Specific) Pg 5-3, third line: Again, the word "fractures" could be replaced by "cracking." A specimen fractures but once; it cracks many times.

Resolution: Good point. Here, too, "crack extension" seemed the better choice over "cracking."

Comment 45: (Specific) Pg 5-3: Loss' Philadelphia paper (E-P Symposium, Oct. 1981) suggests a size effect in R curves with the larger specimen giving a higher R curve. This phenomenon is unexplained at present.

Resolution: At the time the comment was written, the point being made was both interesting and correct. Later conversations with Dr. Loss led the author to believe that the observation has been explained in a straight-forward way. No change in text is necessary.

Comment 46: (Specific) Figures 5.2 and 5.3: These should be credited as NRL data.

Resolution Because the basic NUREG was prepared from the supporting appendices, it seemed more appropriate to cite Appendix D as the source of Figures 5.2 and 5.3 (and 5.4).

Comment 47: (Specific) Figures 5.9 and 5.11: The upper line labeled "GE/EPRI Data" may be too high based on estimates by Loss and Berggren. It should be determined whether the GE experimental data contain a correction for crack extension. This correction will lower the J-R curve. In addition, the NRL data are for 200°C. It is known that the R curve falls with rising temperature. This fact is not mentioned in the comparison with vessel tests.

Resolution: Technically, the commentor probably is on firm ground. The figures cited, however, were taken directly from Appendix H and, as mentioned previously, the reports from the Technical Team members were accepted as the work of the individual authors. Therefore, no modification of the figures is warranted. As for the fact that the J-R data provided by NRL were not obtained at the same temperature as the ITV experiments, that may be less of a factor than is their not being exactly the same material. The last sentence on pg 5-14 should be enough to give fair warning that no pretense at precision was claimed.

Comment 48: (Specific) Pg 5-18: What is a "coupled J value"? Where is it shown on Figure 5.12?

Resolution: "Coupled" is a typographical error; "doubling" is correct.

Comment 49: (Specific) Pg 5-22, Figure 5.13: Is this the preferred figure or should Figure 14 on pg D-23 be used? The latter was used in Appendix I. In the title to this figure, the words following "correlation" should be deleted.

Resolution: The graph shown on pg 5-22 of NUREG-0744 is the one intended for Figure 5.13. It is not clear what was meant by the idea of "preferred" in the question. The lower bound correlation that is preferred as a part of an RPV analysis at any point in time will be that which is based on the most extensive collection of accurate, relevant data. The sentence on pg 5-22 that preceedes Figure 5.13 addressed that point. The figure title has been changed.

Comment 50a: (Specific) The units for J in Figure 5-13 are wrong. The implications of this figure should be discussed in some depth, because it appears to be a key figure with regard to the proposed analysis.

Resolution: The problem with the units is a typographical error. It is believed that there is enough discussion of the implications of Figure 5.13, when all the discussions in the appendices are considered.

Comment 50b: (Specific) Pg 5.22, Figure 5.13: In the title to this figure, the words "for Leak-Before-Break" should be deleted.

Resolution: The figure title has been changed.

Comment 51a: (Specific) The statement that the J/T loading line for a small crack in a large structure is approximately a straight line is unproven and may be incorrect in general.

Comment 51b: (Specific) Figure 5.1 on pg 5-2; Figure 5 on pg B-13: The physical implications of the linearity of the J-T (applied) relationship are not clear.

Resolution: The mathematical derivation of equations for the elastic-plastic loading curves of J = f(T) should be clear to those trained in mechanical analysis. The derivation from an elastic viewpoint (pg 5-6 ff of NUREG-0744) is simple. The result, Eq (5-3)

$$J/T = a\sigma_o^2/E$$

must be recognized as a straight line for a = constant. If so, it should be established as proven for the more-or-less elastic case. Perhaps, more importantly, the above simple example exhibits the characteristic that if there is nonlinearity as a result of crack extension, the slope will increase. Therefore, unstable crack extension (fracture) predicted on the basis of the intersection of the materials J = f(T) curve with the linear load line will be conservatively low with respect to the more accurate loading curve that includes a change in crack length. Hopefully this discussion also will help clarify the question of the physical implications.

Comment 52: (Specific) The flaw size factor is missing from Eq (5-11).

Resolution: The comment appears to be in error; no factors are missing from Eq (5-11).

Comment 53: (Specific) The statement that plastic zone size corrections do not alter Eq (5-13) disagrees with the exact derivation given in Appendix B. This requires a clarification in Vol. I, because exact derivation obviously needs no apology.

Resolution: The author believes that the comment refers to the statement on pg 5-8 of NUREG-0744: "Consideration of plasticity corrections on { } led to the same final result and the conclusion that the same loading line equation applies whether a crack tip plastic zone size correction is used or not." The truth in the quotation lies in the fact that the stress bracket, { }, does not appear in Eq (5-13), having been cancelled out by taking the ratio of J over T. Therefore, there is no disagreement with Appendix B (which says the same thing in different words), nor is any clarification required. The tag-along statement about exact derivation just has no bearing on the issue.

Comment 54: (Specific) What is the basis of the pressure-strain curve in Figure 5.8? How does one construct such a diagram for an actual pressure vessel?

Resolution: The P-ε curve of Figure 5.8 was based on measurements taken at failure of the ITVs noted. It is a reprint of Figure 2, Appendix H. The text accompanying that graph in Appendix H should be read. Because the curve was constructed using data from several vessel tests, it is physically impossible to construct such a diagram for an actual pressure vessel.

Comment 55: (Specific) It is very difficult to figure out Table 5.1, on pg 5-12. Apparently, the $F(\sigma/\sigma_0)$ values are from Table 1 of Appendix B, pg B-35, but some of the values at larger σ/σ_0 ratios do not agree.

Resolution: What seemed apparent to the correspondent was wrong. The entire pg 5-12 of NUREG-0744 was a copy of pg H-28, Appendix H, NUREG-0744. Reading the text of Appendix H, which relates to pg H-28, should clear up any difficulty in figuring out Table 5.1.

Comment 56: (Specific) Pg 6-1, Line 6: Certainly, J-R curves should not be required to be developed first, before the need for them is established.

Resolution: The need for J-R curves is established upon the issuance of NUREG-0744, Revision 1, unless the licensee chooses to utilize a different method to perform the safety analysis if and when it must be performed according to the requirements of 10 CFR 50.

Comment 57: (Specific) Pg 6-1, Lines 13 and 14: Thermal anneal should not be regarded as the only solution to the toughness issue.

Resolution: Annealing of an RPV to recover toughness lost from neutron radiation is not the only solution to the toughness issue nor does the section cited say so. The section cited is only a restatement of 10 CFR 50 wherein annealing is established as an option. Another option is decommissioning.

Comment 58: (Specific) Pg 7-2, Section 7.2, Line 5: There is no listing for Reference 20. Should this be Reference 19?

Resolution: Yes.

Comment 59: (Specific) Pg. 8-2: Reference 8 is not published by ASME, but by the Welding Research Council.

Resolution: The correction was made.

Comment 60: (Specific) Appendix B presumably contains the technical basis for the proposed J-T methodology. The assumptions used in the derivation are not all clearly delineated, and the limitations of the method are not pointed out at all.

Resolution: Appendix B was submitted to the ASTM for publication with very few (only minor editorial) changes. It was given careful review by competent judges and, after a few minor changes, will be published as part of the Proceedings of the Symposium on Elastic-Plastic Fracture Mechanics held in Philadelphia, PA, in October 1981. The successful review by peers in fracture mechanics establishes the merit of the document. Therefore, the staff believes no changes are necessary.

Comment 61: (Specific) Pg B-2: The use of the J/T diagram does involve some new assumptions. These include: (a) the use of the LEFM shape factor and (b) neglect of the Δa value in calculating the critical value of J.

Resolution: The statements are correct.

Comment 62: (Specific) Appendix B: Eqs (2) and (34) are inconsistent. The term, $\bar{\alpha}$, appears in one equation but not in the other.

Resolution: There is no inconsistency. Eq (2), sans α , is part of an explanation of what was done by others (an historical, background discussion). Eq (34) was used by the author of Appendix B as one in a series of examples of how to account for plasticity and work hardening in the fracture. Another variant, Eq 38, used an even more complicated stress-strain formulation.

Comment 63: (Specific) Appendix B: In References 15 and 16, the factor, f*, appearing in Eq (35) is not constant but varies with a/w. There is no explanation here concerning how or why that variation can be neglected. This comment may not be too important for through cracks in long cylinders, but it is definitely relevant to Eq ([illegible]), because the original f* values undergo considerable variation within the range $o < a/w < 1/2$.

Resolution: First, refer to the Resolution of Comment 6e. Although f* is a function of a/w, the analysis assumes that the crack size is quite small relative to the structural dimensions, that is, $a/w \rightarrow o$.

Comment 64: (Specific) Appendix B: The statement that Eqs (42-45) should be restricted to avoid yielding of the uncracked ligament needs rewording. It is the stress bracket term in Eq (42) that needs restricting. Eqs (51) and (52) are intended for substitution into Eq (42) at higher stress levels. This is the basis for Riccardella's verification calculations.

Resolution The comment takes the author of Appendix B to task. Because the section of the text cited (that which follows Eq (45) in Appendix B), clearly notes that the analysis to that point is an elastically derived approximation, it does not seem important to argue a matter of interpretation. The later equations mentioned are not so much substitutions to Eq (42) as the essence of the plasticity analysis for the surface part-through crack in a shell.

Comment 65: (Specific) Appendix B: What of γ is supposed to be used in Eq (46)?

Resolution: The factor cited need not be evaluated for the purpose of determing J/T at instability because it cancels out on taking that ratio to obtain Eq (49).

Comment 66: (Specific) Appendix B: $F(\frac{a}{c})$ does not always diminish with increasing "a." HSST ITV data show that the entire flaw can grow, in which case $F(\frac{a}{c})$ can remain constant.

Resolution: The comment is correct. In fact, it is easy to think of a situation where c will enlarge faster than a. However, the argument in Appendix B (pg B-29 and related sections) is that for increases in a which are large compared to increases in c, the derivatives of F(a/c) can be neglected with the result being a conservative (larger than real) estimate of $T_{applied}$. Therefore, no change is necessary.

Comment 67: (Specific) Pg B-12, the line below Equation (19): The word "or" should be "of."

Resolution: The comment is correct.

Comment 68: (Specific) Pg B-31, the paragraph under the heading C, The Stress Bracket for the Surface Flaw: It would be useful to the reader to note that the meaning of the first sentence is that the geometry correction for the surface flaw can be taken as the Appendix A, Section XI, values for M and Q, where Q is that for $\sigma = 0$. Specifically, that the geometry correction is equal to M^2 divided by Q.

Resolution: The suggested modifications have been made.

Comment 69 (Specific) Pg B-36, penultimate sentence: This appears to be a very important observation which does not seem to be repeated elsewhere. The correctness of the J-$T_{material}$ analysis has been demonstrated up to the experimental J levels where omega is greater than 5. It is not clear that J-$T_{applied}$ analyses must be so restricted, particularly when the stress contribution to J includes other than primary stresses. However, accepting that this restriction is valid, we have the basis for the comment regarding the third paragraph, fourth sentence on pg 6-1.

Resolution: The comment shows that the correspondent read the report with understanding. Responses to several previous comments have discussed the reason for wanting ω to be large within the scope of the analysis and how such considerations bear on the conservative, J/T = 50 in.-lb/in.^{2}, loading line choice.

Comment 70: (Specific) Pg B-37: Data are now available with uncracked ligaments of 4 in. as opposed to 1 in. (Philadelphia paper, NRC information meeting). Thus, Eq (57) gives J/T = 200 in.-lb/in.^{2}

Resolution: The welcome additional data can be used to assess the degree to which J-controlled growth may be expected along the conservative load line where J/T = 50 in.-lb/in^2. Taking the values given in Eq (57), pg B-37, but letting J/T = 50 in.-lb/in.^{2}, using the suggested ligament length of 4 in., and solving for ω, the result is a value of 20. That is large enough to ensure J-controlled crack growth since a value of 10 is considered to be the criterion.

Comment 71: (Specific) Pg B-38: The J_{50} values would probably be highly conservative for application to reactor vessel analyses. For example, data from a 4T CT specimen on A533 steel with USE of 81 ft-lbs produced J = 10,000 in.-lb/in.^{2} with $T \approx 30$, which still is below the J/T = 500 line ($T \approx 20$ at J = 10,000 in.-lb/in.^{2}). In Figure 13, pg B-39, J_{50} = 1400 in.-lb/in.^{2} for a USE 78 ft-lb. steel, a factor of 7 less than the above experimental J value. The margin would probably be less for low shelf steels. For example, a 1018 steel with USE 35 - 40 ft-lbs yielded a J of at least 2000. It is not clear that the true conservatisms of the proposed criterion have been assessed. Further evaluation of the approach using J_{50} (measured or from USE correlation) is recommended.

Resolution: The analysis is warmly received. Rather than use this vehicle as a forum for discussions of safety factors and conservatisms, the staff will defer the issue, as in previous discussion, awaiting the results of the task underway by Section XI of the ASME Code Committee. The author certainly agrees that further evaluation is recommended.

Comment 72: (Specific) Pg C-5, Lines 11-13: Appendices D and E-II discuss side grooving, but not Appendix C.

Resolution: The corrections have been made.

Comment 73: (Specific) Pg. C-7, last sentence: Only the second of these two plots appears in Appendix D.

Resolution: The correction has been made.

Comment 74: (Specific) Pg C-14: In Eq (38) where $(1 + 5/4)\ \lambda^2)^{\frac{1}{2}}$, the closure parenthesis between 5/4 and λ^2 should be removed.

Resolution: There was a typographical error in this equation. However, rather than the deletion of a parenthesis, the appropriate action was the insertion of a parenthesis before 5/4. The correction has been made.

Comment 75: (Specific) Pg C-35, the typed line below Eq (110): The third word should be "partial."

Resolution: The correction has been made.

Comment 76: (Specific) Pg C-29: Line just prior to Eq (88) where $(\lambda_y \leq \lambda_f \lambda_s)$ should be $(\lambda_y \leq \lambda_f < \lambda_s)$.

Resolution: The correction has been made.

Comment 77: Pg C-36, last typed line: Should Eq (114) be Eq (112)?

Resolution: Yes.

Comment 78: (Specific) Pg C-42, third line: The last word should be "satisfies." In Eq (132) there is a geometry term missing.

Resolution: The corrections have been made.

Comment 79: (Specific) Additional J-R curve data on HSST weldments have been generated since NUREG-0744 was drafted. However, these data do not change the basic conclusions. Including the new data would be nice but it requires some rewriting. It may be sufficient simply to reference the NRL work. (See the ASTM and Water Reactor Safety Research Information Meeting Documents enclosed.)

Resolution: The staff appreciates the comment and agrees with the conclusion.

Comment 80a: (Specific) Pg D-2: In Eq (1), the denominator reads 2B; this should be bB.

Comment 80b: (Specific) Pg D-2: Eq (1) should read as follows: $J = \frac{1 + \alpha}{1 + \alpha^2} \cdot \frac{2A}{Bb}$

Resolution: The comments were correct. However, in revising this appendix, the authors deleted that equation.

Comment 81: (Specific) Appendix D, Eq (5). Should not the coefficient be 1000?

Resolution: Yes. (It is now, by the way)

Comment 82: (Specific) Pg D-13: Section 3.4.4 states that nickel, up to 1%, reinforces the deterimental effects of copper for radiation doses. Welds with low copper and low nickel content have lower radiation sensitivities. However, no mention is made in the NUREG of the effect of higher percentages of nickel in the base metal. Very low initial NDT temperatures can be achieved for base metals with high nickel contents. It is recommended that this item be evaluated.

Resolution: The recommendation has been forwarded to the NRC Office of Regulatory Research for consideration.

Comment 83: (Specific) Pg F-14, Lines 18 and 19: Something is missing.

Resolution: No, nothing is missing. The commentor seems to be taking exception to the style of writing of the author of Appendix F.

Comment 84: (Specific) Pg H-4, Figure 2: For V7, T is given as 1.96°F; this might be 2°F, but the two significant figures are questionable.

Resolution: Although the staff agrees with the point made by the commentor, the author of Appendix H was quoting data as they were given and the numbers are correct.

Comment 85: (Specific) Pg H-4, Figure 2: The failure point for vessel V-9 is misplotted. The failure pressure is correct, but the cylinder strain is 1.05%. Correcting this error reveals that the failure data for the intermediate test vessels do not all plot near a bilinear curve.

Resolution: Although the error impacts the overall picture of unity of the ITV failure data, the fact that vessel V-9 was one with the nozzle flaw means that we can afford to overlook the error because the problem deals with flaws in the vessel cylindrical shell where the V-9 results play no role.

Comment 86: Pg H-14 text and Table 5: The failure pressure for vessel V-1 listed in Table 5 is in error. It should be 28.8 ksi. All three predicted failure pressures listed in Table 5 are inaccurately scaled from Figure 2. These values should be 28.8, 29.7, and 31.0 ksi for vessels V-1, V-3, and V-6, respectively.

Resolution Thanks is due to respondent for supplying the corrected data and calculated (scaled, as he said) values. Using his numbers, the predicted and measured pressures for V-1 are identical (28.8 ksi). For vessel V-3, his value of 29.7 ksi should be compared to the measured pressure of 31.0 ksi; for vessel V-6, his value of 31.0 ksi should be compared to the measured pressure of 31.9 ksi. As a result, the conclusion that "the predicted failure conditions are all reasonably conservative under predictions of the actual failure conditions" remains valid with an additional measure of certification.

Comment 87: (Specific) Pg H-6, Table 2: There is general agreement with $\varepsilon/\varepsilon_o$ in Table 2 and Figure 5 (pg I-32) at small values of the stress correction term. There is a substantial difference between $(\varepsilon/\varepsilon_o)$ results in Table 2 and those shown in Figures 3 a-c and 4 a-c (pg I-30 and 31) for large value of $[F(\sigma/\alpha_o)]$. The difference may be a result of the strain range over which the Ramberg-Osgood equation was solved or a result of engineering stress-strain being used instead of true stress-strain. An example of a plot of $\varepsilon/\varepsilon_o$ versus $[F(\sigma/\sigma_o)]$, based on engineering stress-strain, is shown in Figure II-1 below.

Resolution: The correspondent probably is correct; the comment is appreciated.

Comment 88: (Specific) Pg I-iv, Line 16: Omit the word "pressure."

Resolution: The correction has been made.

Comment 89: (Specific) Pg I-3, Line 15: Substitute "similar" for "shallower."

Resolution: The correction has been made.

Comment 90: (Specific) Pg I-3, Lines 15-17: Omit the last sentence.

Resolution: The correction has been made.

Comment 91: (Specific) Pg I-4, Line 5: Change "three" to "two."

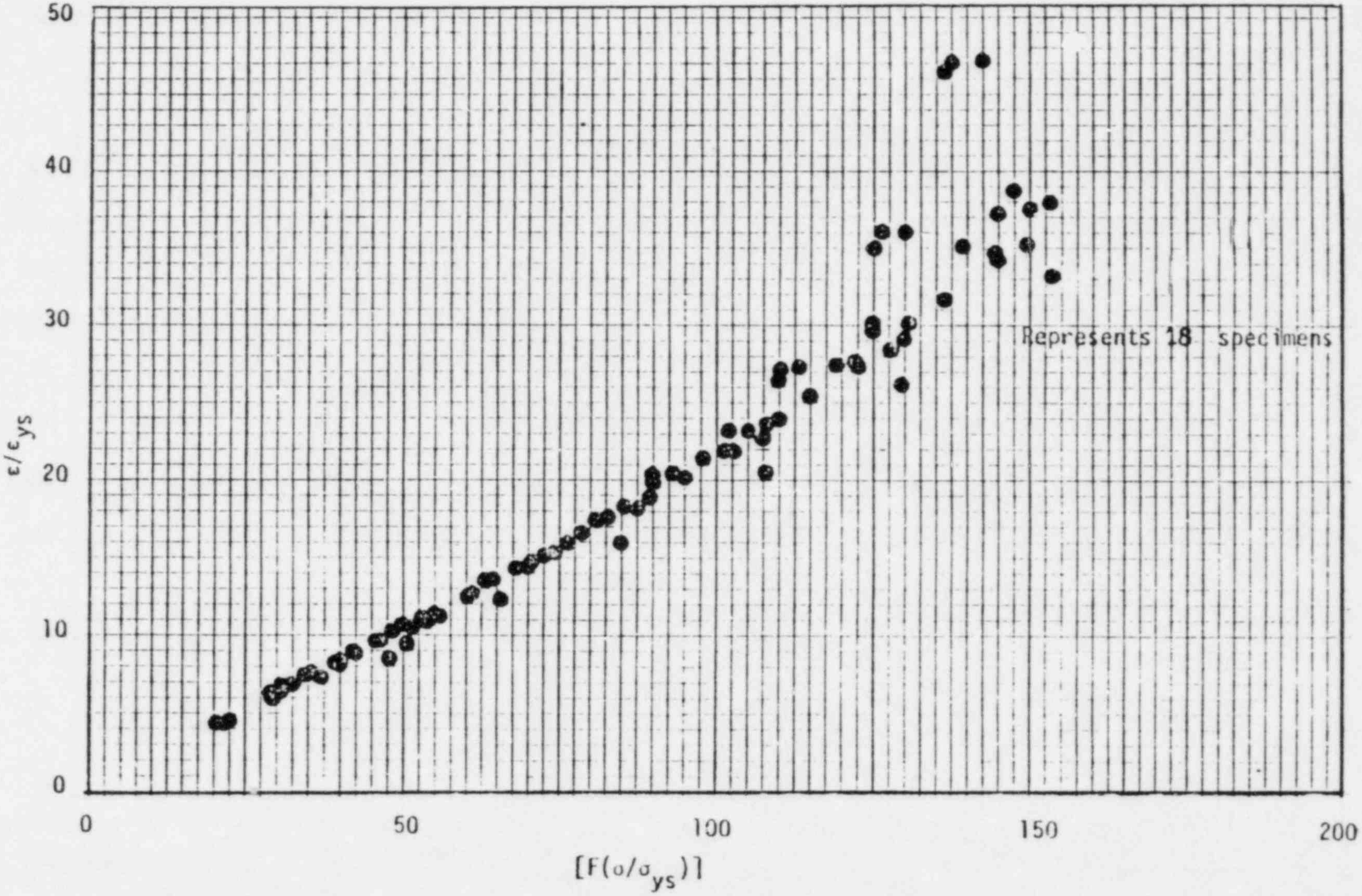


Figure II-1. Plot of strain rates $(\varepsilon/\varepsilon_{ys})$ versus the stress function $[F(\sigma/\sigma_{ys})]$ for the strain ranging from 0.003 to 0.013

Resolution: The correction has been made.

Comment 92: (Specific) Pg I-5, heading for paragraph 2.4: It would be much clearer to the reader if the word "failure" were substituted for the word "allowable" in the title and throughout this discussion. The reason for this distinction is that a factor of safety has not been used in these computations, but the absence of the factor of safety is not clear throughout the discussion.

Resolution: The recommended changes have been made.

Comment 93: (Specific) Pg I-6, first line: The correlation being used here is that in Appendix D rather than that in the basic text. Again the question is asked, which correlation is it intended that the NUREG user apply in such computations?

Resolution: The answer is the same as that given in response to Comment 49.

Comment 94: (Specific) Pg I-8: The first equation in Section 3.1.1 should be as follows:

$$b/a' \, \Psi_1 \left(\frac{a}{b}, \frac{p}{p_o} \right) = \frac{b}{a'} \; \frac{a'}{b} \; (1 - a'/b) \; \Psi \; g_1 \; (a'/b, \; n = 1)$$

Resolution: The corrections have been made.

Comment 95: (Specific) Pg I-9, Eq (3) should be as follows:

$$\varepsilon_t/\varepsilon_y = \left(\frac{\sigma}{\sigma_{ys}}\right) + \alpha \left(\frac{\sigma_t}{\sigma_{ys}}\right)^n \qquad (3)$$

Resolution: The corrections have been made.

Comment 96: (Specific) Pg I-11, Line 4 of first full paragraph: Omit "for."

Resolution: The correction has been made.

Comment 97: (Specific) Pg I-12, last paragraph: The conclusion drawn here is in conflict with the conclusion stated on page B-33 above Eq (51). See also Comment 102.

Resolution: The conflict alluded to seems to be that the text on pg I-12, last paragraph, carries the advice to use LEFM for values of the strain ratio, $(\varepsilon_t/\varepsilon_{ys}) \leqq 0.7$, whereas the text on pg B-33, at the top of the page, directs the reader to use the stress bracket correction for plane strain, as given in Figure 10, for stress levels up to 2/3 of yield. The NUREG author supposes that the commentor viewed the sections cited to be at variance because the advice in Appendix I was to use an elastic analysis, whereas that in Appendix B was to employ a plasticity correction. The appearance of a conflict has resulted because the author of Appendix I assumed that the reader would be sufficiently familiar with fracture mechanics to understand that LEFM analyses can include first-order corrections for crack tip plastic zone sizes. Specifically, Irwin's approximation is used:

$$r_y = \frac{1}{2\pi}(K/\sigma_{ys})^2, \text{ for plane stress, or}$$

$$r_y = \frac{1}{6\pi}(K_I/\sigma_{ys})^2, \text{ for plane strain.}$$

The given relationhips were derived in McClintock and Irwin, 1965. The fracture mechanics analysis recommended by the ASME Boiler and Pressure Vessel Code (always called a "linear elastic fracture mechanics" approach) includes a crack tip plasticity correction factor. More details can be found in Marston, 1978, particularly on pg D-3 and D-4. The advice in Appendix I is to be interpreted as tacitly including an r_y factor in an LEFM approach. The plastic zone size correction from which Figure 10, Appendix B, was derived was the Irwin approximation. The only difference between the passages cited is that one recommends a crack tip plasticity correction factor explicitly and the other does so implicitly. No change in the NUREG is required.

Comment 98: (Specific) Pg I-13: Eqs (8) and (9) are not numbered.

Resolution: The numbers have been added.

Comment 99: (Specific) Pg I-16: In Eq (11), the quantity a/t is to the 2p power.

Resolution: The correction has been made.

Comment 100: (Specific) Pg I-17: This equation should be expressed as follows:

$$J = \frac{\sigma_{ys} \quad \varepsilon_{ys} \quad a \left\{ F\left(\frac{\sigma}{\sigma_{ys}}\right) \right\} R^2}{1 + 1.47\ (a/c)^{1.64}}$$

where

$$R^2 = (1.13 - 0.1\,\frac{a}{c}) + \left[\left(\frac{a}{t}\right)^{1.77} - 0.11\ (\frac{a}{t})^{3.54}\right] \sqrt{\left[1 + 1.47\ (\frac{a}{c})^{1.64}\right] c/a}$$

$$- (1.13 - 0.1\ a/c)\ (a/t)^{1.77}$$

$$\frac{\partial J}{\partial a} = \frac{\sigma_{ys} \quad \varepsilon_{ys} \left\{ F\left(\frac{\sigma}{\sigma_{ys}}\right)\right\}}{1 + 1.47\ (\frac{a}{c})^{1.64}} \left[1 - \frac{(\frac{a}{c})^{1.64}\ (2.41)}{1 + 1.47\ (\frac{a}{c})^{1.64}}\right] R^2 + \frac{2\sigma_{ys}\ \varepsilon_{ys}\ a \left\{ F\left(\frac{\sigma}{\sigma_{ys}}\right)\right\}}{1 + 1.47\ (\frac{a}{c})^{1.64}}$$

$$R \left\{ \frac{-0.1}{c} + \frac{\left\{ \frac{1.27c}{t^{1.77}\ a^{0.23}} - 0.34\ \frac{c\ a^{1.54}}{t^{3.54}} + \frac{3.07a^{1.41}}{c^{0.64}\ t^{1.77}} - 0.63\ \frac{a^{3.18}}{c^{0.64} t^{3.54}} \right\}}{\left[1 + 1.47\ (\frac{a}{c})^{1.64}\right] c/a} \right.$$

$$\left. + \frac{0.277}{c}\ \left(\frac{a}{t}\right)^{1.77} - \frac{2.00\ a^{0.77}}{t^{1.77}} \right\}$$

Resolution: The corrections have been made.

Comment 101: (Specific) Pg. I-18: In Section 3.4, σ_o should be:

$$\sigma_o = \frac{669 + 745.2}{2} = 707.1\ MP_a\ (102.5\ ksi)\ \text{at RT}$$

Resolution: The correction has been made.

Comment 102: (Specific) Pg I-19· There are errors in the bracketing of the equations on this page. In the first equation, the last square should only apply to the M, and when the numerical substitution is made there should be a square showing on the quantity 1.18. The use of the Q = 1.04 value in this computation rather than the value Q = 1.22 is questioned. The latter value is consistent with the recommendations of Appendix B. The effect is very significant, the 0.462 value jumping to 0.542. This is noted on the next page, but it is sort of hidden, and all the tables are done on the more conservative basis. At the bottom of this page the example calculations are made following two approaches. It seems that the preferred approach, using Eq (51) from Appendix B, should also be included. If this done, the failure stress for the two Q values becomes 36.8 and 39.8 ksi, respectively. These are much closer to the results obtained by Approach 2 than are the results obtained from Approach 1. Approach 1 is questionable because of the difficulty in reading curves in these low strain ranges. It should also be noted near the top of this page that the computations have been performed for a temperature of 543°F.

Resolution The basis for choosing a value of Q = 1.04 stemmed from the desire to include a significant amount of prefracture crack tip plasticity in the analysis. The value of Q = 1.04 is related to a nominal stress equal to the yield strength, $\sigma/\sigma_{ys} = 1$, which is at the upper limit of LEFM crack tip plasticity corrections. Because of that decision, it would seem to be inappropriate to use, as the discussor suggested, Eq (51) on pg B-33, Appendix B. However, it is instructive to pursue the suggestion. The authors of Appendix I, in response to the comment, have submitted the following additional calculational results.

Calculation	Q	Plastic zone corrected	σ, ksi	Comments
1.	1.22	No	34.9	Noted on pg I-20
2.	1.22	Per Eq (51) using 1/6; plane strain	32.96	
3.	1.22	Per Eq (51) using 1/2; plane stress	29.1	
4.	1.04	Included in Q	31.0	cf. Lines 2, 3

The authors agree with the correspondent that if one were to choose 1.22 as the value for Q, the resultant value of F would be 0.542, rather than 0.462. However, it may be that the commentor made some arithmetic error, making the effect on the calculated fracture stress much greater than it actually is. Comparison of the results of calculations 2 and 3 (32.96 and 29.1 ksi, respectively) with the values of 36.8 and 39.8 ksi reported in the comment shows the magnitude of the error. The comment accompanying calculation 4 is intended to call attention to the fact that the result reported in Appendix I (pg I-20) is bracketed by the plane strain and plane stress results. The authors and the NRC staff believe that the original calculations are acceptable. Note that the statement in the second sentence of the bottom paragraph on pg I-20 calls attention to the fact that the elastic-plastic relationships used came from some specific data and some arbitrary choices in how to display it. Other analyses, directed toward other specific situations, could rightfully use a curve somewhat different than that drawn on Figure 5. As to the suggestion that the computations reported in Appendix I relate to a temperature at 543°F, it is not clear where that temperature value came from. The temperatures reported on Figures 3, 4, and 5 were room temperature and 550°F. It does not matter, however, since the effect of temperature over that range was quite small, as the data plotted on Figure 5 show.

Comment 103: (Specific) Pg I-20: The equation for σ should read

$$\sigma = 1.12 \times 10^{-3}[191{,}130 \text{ MPa}(27{,}700 \text{ psi})] = 213.9 \text{ MPa}(31.0 \text{ ksi}).$$

Resolution: The correction has been made.

Comment 104: (Specific) Pg I-21, Lines 1-4: This statement should be rewritten to say the comparison in Table 4 suggests a good agreement between the results obtained from LEFM and that based on Eq (9). Also, the use of Eq (9) will predict a conservatively low value of allowable stress when compared to that predicted by LEFM.

Resolution: The recommended changes have been made.

Comment 105: Pg I-30, I-31, and I-32, Figures 3, 4, and 5: If these figures are to be useful to the user of the NUREG, they should be drawn to a bigger scale.

Resolution: The NUREG author apologizes for any inconvenience the scale of the figures may have caused, but that is the way the photo-ready copy was received. A request to the author* no doubt would be enough to obtain larger copies of the graphs.

Comment 106a: (Specific) Pg I-47, Table 4, Note (2): Reference 8 is not correct; suspect that reference should be made to Page D-23.

Resolution: Note (2) has been changed to read "Appendix D."

Comment 106b: (Specific) Pg I-47: The title of Table 4 should read "Allowable Stress for Appendix G Type Surface Flaw[1] Using Equation 9," rather than "...Using Tearing Modulus Approach."

Resolution: The correction has been made.

Comment 107: (Specific) Pg J-2, Table 1: In the first column near the bottom, the symbol alpha should be replaced by the letter "a." In the same row, the alignment between the second and third columns is not correct. The number 1.3 should be on the same line as the words "Full Pressure."

Resolution: The corrections have been made.

Comment 108: (Specific) Pg J-5, penultimate paragraph: The present penultimate sentence should be changed to read: "If the pressure is maintained during

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*Dr. Walter G. Reuter, EG&G Idaho, Inc., Fuels and Materials Division, P.O. Box 1625, Idaho Falls, Idaho 83401.

cooldown and the conditions are acceptable, the nominal factor of safety is computed as 1.8." New sentences should be inserted at this point reading: "The same factor of safety is obtained if safety is obtained if the pressure is 0 at the end of the cooldown and the toughness has obtained a minimum value, because 26.8 divided by 15 is equal to 1.8. Any intermediate condition gives a factor of safety value between 1.3 and 1.8."

Resolution: The recommended changes have been made.

Comment 109: (Specific) Pg J-6: The first few lines should read: "...at temperatures higher than the lowest temperature permitted for application of the full operating temperature, 2250 psi, the vessel material will be sufficiently ductile...," etc. In the penultimate paragraph, the seventh line, second word should be "must."

Resolution: The recommended changes have been made.

Comment 110: (Specific) Pg J-7, last paragraph, fourth line: The word should be "postulated."

Resolution: The correction has been made.

Comment 111: (Specific) Pg J-9, Item (3): The text assumes that the revised Appendix G has been issued, which it has not. Therefore, this should be revised to read: "10 CFR 50, Appendix H, requires that the RT_{NDT} shift be evaluated at the more conservative of 50 ft-lbs or 35 mils lateral expansion without any basis for measurement at these levels." In Item (4), the end of the first sentence should read "curve cannot be defined" rather than "curve may not be adequate."

Resolution: The section of the Code at issue is 10 CFR 50, Appendix G; to that extent the portion of the text cited has been corrected. At the time of publication of NUREG-0744, For Comment, the new Rule had not been published and the comment has merit in that it was presumptuous to think that the Code revision would be out quickly. At the time of this writing, it is before the NRC Commissioners for review; thus it should be published in the Federal Register soon. In the revised version of 10 CFR 50, Appendix G, the subject is covered in Section II.E in the following way. The adjusted reference temperature is to be measured at the 30 ft-lb (41J) level in the average Charpy curve for the irradated material relative to that for the unirradiated material. Thus, the change recommended in the comment would also be in error relative to the soon-to-be published revision. Considering the role of the cited section in Appendix J, no further editing is necessary. The changes suggested for Section 3.1(4) would distort the intended meaning.

Because attention was called to the section, however, a clarification was warranted and has been made.

Comment 112: (Specific) Pg J-15, third line from the bottom: "NRC" should be "NRL."

Resolution: The correction has been made.

REFERENCES

Barsom, J. M.; and S. T. Rolfe, "Impact Testing of Metals," ASTM STP-466, p. 281, 1970.

Broek, David, "Elementary Engineering Fracture Mechanics," Noordhoff International Publishing, Leyden, 1974, p. 122 ff.

Dieter, George E., Jr., Mechanical Metallurgy, McGraw Hill, New York, 1961.

Electric Power Research Institute, "Methodology for Plastic Fracture," March 1981.

Kumar, V. et al; "An Engineering Approach for Elastic-Plastic Fracture Analysis," Electric Power Research Institute/General Electric Report EPRI NP-1931, July 1981.

Loss, F. J., "J-R Curve Characterization of Irradiated Low Upper Shelf Welds," 9th Water Reactor Safety Research Information Meeting, National Bureau of Standards, Gaithersburg, MD, October 26-30, 1981.

Loss, F. J., J. G. Merkle, A. L. Hiser, and H. E. Watson, "J-R Curve Characterization of Irradiated Low Shelf Nuclear Vessel Steels," ASTM Second International Symposium on Elastic Plastic Fracture Mechanics," Philadelphia, October 6-9, 1981.

Marston, T. V., ed, "Flaw Evaluation Procedures," EPRI Report NP-719-SR, Palo Alto, August 1978.

McClintock, F. A. and G. R. Irwin, "Plasticity Aspects of Fracture Mechanics," ASTM STP 381, pp 84-113, 1965.

Rolfe, S. T., and S. R. Novak, "Review of Developments in Plane Strain Fracture Toughness Testing," in ASTM STP-463, 1970.

U.S. Nuclear Regulatory Commission, NUREG/CR-0859, J. C. Williams, "Ductile Fracture Toughness of Heavy Section Pressure Vessel Steel Plate," September 1979.

---, NUREG-0939, Merkle, J. G. and R. E. Johnson, "Some Elastic-Plastic Pressure Vessel Sample Calculations," to be published.

APPENDIX A

Task A-11
Revision 3

REACTOR VESSEL MATERIALS TOUGHNESS

Lead NRR Organization:	Division of Safety Technology (DST)
Lead Supervisor:	Karl Kniel, Chief Generic Issues Branch, DST
Task Manager:	Richard Johnson, Generic Issues Branch, DST
Applicability:	All Reactors
Projected Completion Date:	August 1981

APPENDIX A

Task A-11

1. DESCRIPTION OF PROBLEM

Because the possibility of failure of nuclear reactor pressure vessels designed to the ASME Boiler and Pressure Vessel Code is remote, the design of nuclear facilities does not provide protection against reactor vessel failure. Prevention of reactor vessel failure depends primarily on maintaining the reactor vessel material fracture toughness at levels that will resist brittle fracture during plant operation. At service times and operating conditions typical of current operating plants, reactor vessel fracture toughness properties provide adequate margins of safety against vessel failure; however, as plants accumulate more and more service time, neutron irradiation reduces the material fracture toughness and initial safety margins.

Results from reactor vessel surveillance programs indicates that up to approximately 20 operating PWRs will have beltline materials with marginal toughness, relative to the requirements of Appendices G and H of 10 CFR Part 50, after comparatively short (approximately 10 EFPY) periods of operation. The specific requirement which may be violated is that of paragraph V.B, Appendix G, 10 CFR Part 50. For vessels which fail to satisfy that requirement, paragraph V.C.3, Appendix G, 10 CFR Part 50, must be satisfied (along with the rest of V.C); that is, perform an analysis which demonstrates the existence of adequate operational safety margins against fracture. For plants currently under licensing review, reactor vessels generally have acceptable fracture toughness. However, a few plants under licensing review have reactor vessels that have been identified as having the potential for marginal fracture toughness within their design life; these vessels will have to be reevaluated in the light of the new criteria for long term acceptability.

The fundamental goal of Task A-11 is to provide an engineering method to assess the safety margin for failure prevention in nuclear reactor pressure vessels. The method will employ the most advanced fracture mechanics concepts presently available. Although linear elastic fracture mechanics analyses may be applicable at low temperatures, the amount of crack tip plastic deformation accompanying fracture at high temperature will be relatively large, even in pressure vessel steels of low toughness. Therefore safety will be evaluated by comparing some measure of fracture resistance to a structural parameter, both being based on elastic-plastic fracture mechanics concepts. The concepts set forth in NUREG-0311, "A Treatment of the Subject of Tearing Instability," will be utilized to develop the required engineering method. Adequate margin will require that the structural parameter remain sufficiently below the measure of fracture resistance but the quantitative relationship may depend on the reactor plant conditions. For example, a much larger margin would be required for normal/upset conditions than for low probability accident events.

2. PLAN FOR PROBLEM RESOLUTION

The determination of appropriate licensing criteria for low toughness reactor vessel materials and the evaluation of material degradation resulting from neutron irradiation demands an interdisciplinary effort encompassing several aspects of materials and fracture technology. The plan for development of suitable licensing criteria for low toughness reactor vessel materials, including the effects of neutron irradiation damage, includes the following tasks.

A. Identify and measure the mechanical properties which control tearing instability types of fractures.

B. Develop a method for analyzing structural members that incorporates postulated flaws, under conditions which could lead to tearing instability fractures.

C. Using the results from Subtasks A and B, define reactor vessel safety criteria to avoid failure by tearing instability fracture, to supplement existing criteria for other failure modes.

D. Evaluate the feasibility of in-place reactor vessel annealing to regain toughness.

E. Evaluate actions which could lessen the severity of actual neutron radiation damage or improve the accuracy of calculations of such damage.

F. Establish a computer information system for storage and retrieval of reactor pressure vessel materials data.

Each subtask is discussed briefly in the rest of this section.

A. Evaluate Material Fracture Resistance

The measurement of fracture toughness for reactor vessel and other materials at temperatures corresponding to the upper shelf region is complicated by the presence of significant pre-fracture plastic flow. Current toughness testing methods based on linear elastic fracture mechanics are not adequate to account for plastic flow. New toughness testing techniques have been developed to allow evaluation of low toughness in reactor vessel materials for normal, upset and accident conditions.

It is widely recognized that the J-integral provides a valid, general solution to the problem of crack tip singularity fields under large-scale yielding, even up to fully plastic conditions for some geometries. Moreover, J_{Ic} has been shown to provide a good indication of small-scale crack extension, although the ASTM has not yet established a standard test method for its measurement. More advanced work at Washington University, St. Louis, Missouri, under NRC funding, has resulted in the development of the tearing modulus, T, which is proportional to dJ/da. An experimental method, developed under the Office of Nuclear Regulatory Research funding apparently can be used routinely to provide curves of $J = f(\Delta a)$, the so-called J-R curves. From such data, both J_{Ic} and T_{matl} can be determined. The former has proven adequate as a general fracture parameter; the latter provides a criterion for tearing instability where a large value of T_{matl} indicates ductile tearing and a small value indicates fast fracture.

The goal of this subtask is to provide the relevant materials mechanical property data for the evaluation of reactor vessel margin against fracture at temperatures above the ductile-brittle transition (beyond the range of linear elastic fracture mechanics applicability). Task A-11 will use data provided by the RES (NRC)-funded HSST Program which will include the effects of material condition, temperature and neutron radiation.

B. Develop Structural Analysis Methods

Application of the tearing modulus concept to a reactor vessel failure evaluation requires the development of a method for determining load carrying capacity. Factors to be included in the analytical method must include the following. The geometry of the component must be a basic consideration, including postulated flaw size, shape and orientation, in a parametric way. Crack initiation and propagation will be characterized by J-integral and tearing modulus, T_{appl}, parameters. Loading conditions will include time dependence and the role of structural compliance. Temperature is a consideration to the extent that the instability analysis is applicable only above the ductile-brittle fracture mode transition. At relatively low temperatures the well-developed linear elastic fracture mechanics methods will be applicable.

The problem to be faced when considering reactor pressure vessel welds of marginal toughness is that neutron radiation can decrease the toughness, as represented by the Charpy upper shelf energy, below that required by current regulations. Because of the dominant role of radiation-induced embrittlement, the elastic-plastic response of the reactor pressure vessel beltline region will be controlling and will be the calculation used for the purpose of meeting the Task A-11 goal.

Completion of this subtask will depend on a Technical Assistance contract, funded by NRC and managed by ORNL.

Additional effort is available from existing Technical Assistance contracts with Washington University, St. Louis, Missouri and the Naval Research Laboratory.

This subtask will provide elastic-plastic fracture mechanics formulations, applicable to reactor pressure vessel beltline regions, with which relevant structural parameters can be calculated for comparison to material properties (Subtask A) in order to evaluate failure margins.

C. Define Safety Criteria

To ensure adequate margins against failure for plants with marginal toughness materials in the reactor vessel beltline region, it will be necessary to establish suitable safety criteria for the vessels which fail to satisfy the requirements of Section V.B, Appendix G, 10 CFR Part 50. The solution is to employ the elastic-plastic fracture concepts set forth in NUREG-0311. The relevant materials mechanical properties will be those developed in Subtask A, above. The reactor vessel beltline region will be analyzed with the methods developed in Subtask B, above. The material parameters, such as J_{Ic} and T_{matl} can be compared to the structural parameters, such as J and T_{appl}. Comparison, as was done in the report "A Preliminary Fracture Analysis on the Integrity of HSST Intermediate Test Vessels" by A. Zahoor, P.C. Paris and M.P. Gomez, is expected to show that crack extension occurs when J is the order of J_{Ic} and that the fracture mode depends on the relative mass of T_{matl} and T_{appl} (where fast fractures can be avoided by keeping T_{appl} to values well below T_{matl}). This subtask will provide more realistic criteria for evaluating vessel fracture margins under normal, upset or faulted conditions at higher temperatures than the currently available linear elastic fracture mechanics. The required margin of safety will depend on analyses of available fracture data (such as the HSST vessels) and on the severity of the given operating conditions.

D. Evaluate Vessel Annealing Feasibility

Thermal annealing to recover the toughness lost by neutron radiation was recognized as a theoretically possible method to regain toughness margins. Studies are underway through contracts funded by the NRC and EPRI. The feasibility studies will assess the practicality of reactor vessel recovery annealing. Engineering guidance will be developed to help licensees determine the relative merits of vessel annealing to regain toughness.

E. Radiation Damage Abatement

The root cause of the reactor vessel toughness problem is neutron radiation. There are at least three aspects of neutron radiation which will be examined to determine their potential for reducing the severity of pressure vessel embrittlement or improving the accuracy of embrittlement calculation. The thrust of this subtask is to determine the amount of decrease in calculated mechanical property degradation which could be attained, while maintaining safety margins, by more exact neutron radiation calculations and to evaluate the potential for mitigating the problem through minor design changes.

(1) The neutron fluence through the vessel wall is calculated. Some conservatism is purposely put into the calculations. However, for marginal material, small decreases in calculated fluence could delay the point in time when the current code limits would be violated and, in some cases, could eliminate the problem altogether. The Office of Nuclear Regulatory Research, NRC, has an ongoing program which includes evaluation of neutron flux calculations and measurements. Although the program will not be completed within the term of Task A-11, early results may be used to assess the accuracy and margin of conservatism of vessel embrittlement calculations.

(2) Pre-service estimates of changes in reactor pressure vessel mechanical properties per unit fluence are based on relevant data, including those from test reactor experiments. Vessel surveillance programs, required by 10 CFR Part 50, provide closer approximation from encapsulated specimens close to the vessel wall in the same reactor environment. Surveillance data, as well as some long-term basic radiation experiments, can be used to modify relationships between fluence, inferred from calculation and measurement, and mechanical properties so that the predicted changes will be more realistic. However, test reactor neutron radiation is significantly different from that through the vessel wall, particularly with respect to dose rate and spectrum. The extent to which such results are applicable to vessel steels with marginal toughness will be examined as part of this subtask.

(3) To the extent that neutron fluence reductions can significantly reduce the rate of embrittlement, thereby delaying the advent of code violation, it is worthwhile to consider actions which would diminish the actual flux at the vessel. Shielding, for example, might be inserted between the core and the vessel. Another possibility being considered by some European operators is replacing corner fuel assemblies with dummies thus reducing the azimuthal neutron peaks.

F. Establish a Vessel Data Information System

Because of the large number of possible combinations of reactor vessel and surveillance materials and the large number of variables involved in evaluating these materials, it is necessary to develop an information system for the storage and retrieval of these data. This system will be utilized particularly to maintain up-to-date, accurate data for the generic and plant specific evaluation of operating facilities. This subtask is part of a program funded by DOE/NPD, managed by Sandia, Albuquerque, and is complete.

3. BASIS FOR CONTINUED PLANT OPERATION AND LICENSING PENDING COMPLETION OF TASK

As discussed in Section 1, the safety issue addressed by this task is the reduction of reactor vessel material fracture toughness as a result of neutron irradiation. The operational temperature range includes the transition temperature region, where material toughness increases significantly with increasing temperature, and the upper shelf temperature region, where material toughness reaches a relatively constant maximum value. The task will develop licensing criteria to ensure that adequate margins of safety, relative to flaw-induced fracture, are maintained during normal operating and postulated accident conditions for reactor vessels containing beltline (that part of the reactor vessel directly opposite the core) material with reduced toughness after prolonged irradiation.

For most plants now in the licensing process, current criteria, together with the materials currently employed, are adequate to ensure suitable safety margins for the reactor vessels throughout their design lives. For currently operating plants, and for several plants in late stages of licensing that may have marginal toughness materials, the safety margins required by Appendix G to 10 CFR Part 50 in the transition temperature region are, or will be, maintained during normal operating conditions by appropriate shifts in the operating pressure-temperature limitation. Various analyses of accident conditions indicate that adequate material toughness in the transition temperature region will continue to be available to ensure adequate safety margins for time periods significantly in excess of that required to complete this task.

A few PWRs have reactor vessel beltline materials whose upper shelf energies may fall below levels required by Appendices G and H to 10 CFR Part 50 within the next few years. An interim assessment* was made of the safety margins with respect to flaw-induced fracture for operating vessels with low upper shelf beltine materials. The evaluation indicated that adequate margins of safety can be maintained in the interim period prior to completing this task or the postulated stress and flaw conditions specified in Appendix G to Section III of the ASME Code and required by Appendix G to 10 CFR Part 50.

Pending completion of this task, the safety margins required by Appendix G to 10 CFR Part 50 for operation in the transition temperature region can be maintained during normal operation by appropriate shifts of the operating pressure-temperature limits as dictated by the material surveillance program results and Regulatory Guide 1.99. Initial analyses submitted by some NSSS vendors and our preliminary review indicate that adequate toughness margins can also be maintained in the transition region for postulated accident conditions for up to approximately 20 years of neutron irradiation, or significantly beyond completion of this task.

Appendix G to 10 CFR Part 50 requires licensees of those plants where the beltline material upper shelf energy is predicted to fall below 50 ft-lb to conduct a 100% volumetric examination of the low toughness

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*Memorandum, V.S. Noonan to D.G. Eisenhut, "Reactor Vessels with Marginal Toughness Properties," July 19, 1979.

beltline material. This examination provides added assurance that very large flaws are not present in the reactor vessel beltline region.

Should the results of this task indicate that in the future adequate margins of safety for the reactor vessels of operating plants cannot be demonstrated for both normal operation and postulated accident conditions, one or more of the following alternative measures can be taken.

(1) Reactor vessel annealing to regain material toughness in the beltline region.

(2) Increased beltline inspections using improved in-service inspection (ISI) techniques, as they become available with demonstrated required reliability, leading to a justified decrease in postulated flaw size.

(3) Modifications to the vessel internals or core design to modify the neutron flux and reduce subsequent material degradation.

(4) System modifications to limit the severity of loading (stress levels) of the reactor vessel during postulated emergency or accident conditions.

In summary, the staff considers that in the interim period the safety margins are adequate to ensure the safety of reactor vessels in currently operating plants. The current licensing criteria and the materials used for reactor vessel fabrication provide assurance that reactor vessels for those plants now in the licensing process will also have adequate margins of safety relative to flaw-induced failure. Accordingly, we conclude that while the task is being performed, continued operation and plant licensing can proceed with reasonable assurance of protection to the health and safety of the public.

4. NRR TECHNICAL ORGANIZATIONS INVOLVED

A. Engineering Branch, Division of Operating Reactors. Has overall lead responsibility in the identification of relevant reactor vessel material in licensed plants, evaluation of operating experience with neutron irradiation damage, determination of the associated degradation in reactor vessel material toughness and the evaluation and determination of an appropriate safety criterion for low toughness reactor vessel materials.

Manpower Estimates: 2.0 man-years FY 1980, 0.5 man-year FY 1981

B. Materials Engineering Branch, Division of Systems Safety. Has lead responsibility for review of experimentally determined materials fracture resistance as a function of neutron radiation, for developing the NRC position on in-place reactor vessel annealing and for evaluation of information developed during the evaluation of material toughness in licensed facilities for possible inclusion into material toughness criteria currently used for facilities not yet licensed for operation, where appropriate.

Manpower Estimates: 0.25 man-year FY 1980, 0.1 man-year FY 1981

C. Reactor Safety Branch, Division of Operating Reactors. Has lead responsibility for review of neutron fluence calculation methods. Will advise EB/DOR with respect to the application of the results from the RES radiation damage program to the problem of predicting reactor vessel damage and the advisability of recommending shielding or core modifications to mitigate neutron damage.

Manpower Estimates: 0.2 man-year FY 1980, 0.1 man-year FY 1981

D. Environmental Projects Branch 2, Division of Site Safety and Environmental Analysis. Has lead responsibility for defining licensing criteria related to effluent and personnel exposure control during reactor vessel annealing operations.

Manpower Estimates: 0.04 man-year FY 1980

5. TECHNICAL ASSISTANCE

Technical assistance from organizations outside the NRC will be required to complete Tasks A through F in Section 2, Plan for Problem Resolution, i.e., all aspects of the Task Action Plan. The contractors assisting in these tasks are as follows:

A. Contractor: ORNL (EB/DOR)

Funds Required: $80K FY 1980

The scope of this program includes three tasks. The first is material evaluation wherein available experimental results will be revised to establish relevant fracture mechanics parameters and the effect of size and neutron radiation on them. The second is reactor vessel analyses wherein existing plastic-elastic fracture mechanics concepts will be used to develop a crack instability predictive method applicable to the reactor vessel beltline region and compared to available vessel test data. The third is evaluation wherein the results of the other two tasks will be compared to the criteria of existing codes (ASME and 10 CFR 50). Details of the program are currently being developed by ORNL. This may reveal the need for more funds to complete the work than the current $80K allocation.

B. Contractor: Washington University (EB/DOR)

Funds Required: $80K FY 1980

This program is directed specifically at Tasks 2-A, 2-B, and 2-C. The results of the program will allow advanced fracture mechanics techniques to be used to establish a technical basis for NRC's development of a suitable licensing criterion for low toughness materials. Associated with this is the determination of simplified analytical techniques to evaluate normal operating conditions, postulated accident conditions and assistance in plant specific analyses.

C. Contractor: Naval Research Laboratory (EB/DOR, MTEB/DSS)

Funds Required: $75K FY 1980

This program will investigate neutron irradiation of reactor vessel steels and is directed specifically at Task 2D, Evaluate Vessel Annealing Feasibility. The results should provide improved means to quantitatively describe the effects of material microstructure, chemical composition, neutron spectra and dose rate and allow suitable evaluation, prediction and monitoring of irradiation damage to reactor vessel steels. Included in this program is a study of the feasibility of in-place annealing of reactor vessels to restore fracture toughness to levels that will provide adequate safety margins.

D. Contractor: Brookhaven National Laboratory (RS/DOR)

Funds Required: $5K FY 1980

This program will provide independent neutron flux (fluence) calculations including the effects of core and structural configurations and energy spectra.

6. INTERACTION WITH OUTSIDE ORGANIZATIONS

A. Licensees

Intermittent interaction with licensees is expected for the purpose of obtaining required materials data.

B. NSSS Vendors

Some plant specific analyses have been conducted by the NSSS vendors. Review of the portions of these analyses relevant to completion of the generic task will be required. Some NSSS vendors have first-hand knowledge of fabrication and materials data relevant to low material toughness; review of these data will be required.

C. EPRI

EPRI is currently funding a number of programs related to reactor vessel materials toughness. These programs include studies for neutron irradiation damage of pressure vessel steels and the development of fundamental failure criteria based on elastic-plastic fracture mechanics. Interaction with EPRI to remain informed on the direction and results of these programs and to ensure that appropriate NRC licensing concerns are addressed will be required.

D. ACRS

This task is closely related to one of the generic items identified by the ACRS and, accordingly, will be coordinated with the Committee as the task progresses.

E. Sandia (Albuquerque)/DOE

A program in support of Task A-11 was funded by DOE and managed by Sandia, Albuquerque, during FY 1979. The program had two stated goals: (1) develop an information system to complete Subtask F and (2) develop an analysis for assessing the fracture failure margin of reactor vessel beltline regions using elastic-plastic concepts as described in Subtask B. Contracts were let for both goals. The contractor given the job of developing a computer program for storage and retrieval of reactor pressure vessel data completed the work; the contractor given the job of developing an elastic-plastic vessel analysis could not complete the work within the limits of available time and money.

7. ASSISTANCE REQUIREMENTS FROM OTHER NRC OFFICES

A. Office of Nuclear Regulatory Research, Division of Reactor Safety Research, Metallurgy and Materials Branch.

RES is funding a major experimental research program (Heavy Section Steel Technology, HSST) through Oak Ridge National Laboratory to determine the fracture toughness of reactor vessel steels and the safety margins for reactor vessels. At the request of NRR, RES modified this program to include materials with low toughness, representative of those at operating facilities. (Subtask A)

At the request of NRR, RES is supporting a program to verify experimentally the application of the tearing stability concept as a failure criterion for beltline materials with marginal fracture toughness. (Subtask A)

RES initiated a comprehensive research program to experimentally validate neutron irradiation damage in pressure vessel steels and the associated calculational schemes used to predict radiation damage. This effort is to be part of an overall program being conducted in cooperation with research groups in the US and Europe. (Subtask E)

B. Office of Standards Development, Division of Engineering Standards, Structures and Components Standards Branch.

SD is assisting NRR in the study of the effects of neutron irradiation and the evaluation of low toughness reactor vessel steels. (Subtask C)

C. Office of Management Information and Program Control, Division of Regulatory Information Systems, Processing and Programming Branch.

MIPC provides assistance to NRR toward the goal of establishing a computer-based information system for the storage and retrieval of materials surveillance data. (Subtask F)

8. POTENTIAL PROBLEMS

The technical information required to complete Subtasks A (experimental) and B (analytical) must be developed by advancing the elastic-plastic fracture mechanics state-of-the-art. Therefore, although the contractors and their corresponding NRC Technical Contacts estimate that the work can and will be completed on schedule, unforeseen additional works needs and delays in completion may be encountered as was the case in 1979.

APPENDIX B

A METHOD OF APPLICATION OF ELASTIC-PLASTIC FRACTURE MECHANICS TO NUCLEAR VESSEL ANALYSIS

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Manuscript Submitted: January 1981

A report produced under
subcontract K-8195 for
EG&G Idaho, Inc. By Del
Research Corporation
- FIN A-6419 -

ABSTRACT

The primary purpose of this work was to provide analytical methods for assessing the safety of nuclear reactor pressure vessels against fracture when the material Charpy upper shelf energy may fall below 50 ft-lb because of neutron radiation damage. The approach made use of "tearing instability" concepts under "J-controlled growth" conditions for the crack stability criterion. The above purpose was served by developing fracture mechanics methods of wider applicability than previously available for analysis at upper shelf conditions (above transition temperature). Elastic-plastic fracture mechanics was used to extend recognized linear elastic fracture mechanics flaw analysis equations for through-the-thickness flaws and surface flaws into the plastic range. The approach also made use of J-R curve characterization of the material fracture resistance.

A crack stability diagram in the form of J as a function of T plot was shown to be useful in demonstrating safe levels of loading (applied J) by comparison to the material J-R curve, plotted on the same diagram. Limits of applicability also are readily assessed on the diagram. Consequently, a safe level of applied load, J_{50} (for J/T = 50), was suggested and the possibility of its correlation with upper shelf Charpy energy values discussed.

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ACKNOWLEDGEMENTS

This analysis has been developed and this report produced under a subcontract (K-8195) between the author and EG&G, Idaho (Nuclear) Incorporated. The encouragement of R. Johnson of (NRC) and R. Gamble (formerly with NRC, now with EDS Nuclear) is gratefully acknowledged as essential to this work. Their continued guidance, experienced perspectives, and earlier direct technical support has been most effective and is due utmost thanks. The financial support of other programs at Washington University (St. Louis) by NRC contributed effectively to the origins of the analysis herein. The use of J versus T diagrams, as further developed here, was originated in earlier unpublished work with Societe Technigaz.

APPENDIX B

A METHOD OF APPLICATION OF ELASTIC PLASTIC FRACTURE MECHANICS TO NUCLEAR VESSEL ANALYSIS

INTRODUCTION

The ASME Boiler and Pressure Vessel Code for nuclear reactor pressure vessels has for some time permitted the use of linear-elastic fracture mechanics (LEFM), specifically in Appendix A of Section XI. This has allowed clear and conservative evaluations of any potential danger due to flaws found in inspections of reactor vessels. However, LEFM, as incorporated in the Code, has a limited range of direct applicability without large and perhaps undue conservatism. Moreover, the Code version of LEFM makes use of the K_{Ic} - K_{Id} concept of impending failure (little or no crack growth), instead of the more advanced concepts of flaw or crack stability permitting limited stable flaw growth. Under the use of LEFM, the Code itself acknowledges ranges of inapplicability such as well above the transition temperature where LEFM cannot produce applicable quantitative results. Appendix A provides no specific criteria for upper shelf toughness; the situation was discussed in Reference 1. In Title 10 of the Code of Federal Regulations Part 50 (10 CFR 50), a lower limit is imposed on the Charpy upper shelf energy (USE), namely 50 ft-lb. For materials of less USE, unspecified methods must be used to assure safety. On the other hand, the current ASME Code provisions using fracture mechanics have served very well in cases of appropriate quantitative applicability.

In recent years, a great deal of progress has been made in J-Integral-based elastic-plastic fracture mechanics (EPFM). In particular, a more advanced crack stability criterion has been developed[2,3] and widely accepted[4,5] which depends on the whole J-Integral R curve for material characterization (rather than a single value such as K_{Ic}, which is more limited). These and other advances in EPFM make possible the suggestion of new methods for application to nuclear vessels.

The new methodology presented in this report is proposed on its own merit but is phrased with the existing Code in mind in order to supplement it with alternative methods in areas such as upper shelf conditions where the existing Code seems lacking. Indeed, the most realistic postulated vessel failure conditions are usually well within the elastic range for gross section stresses but may include occasional cases of large scale yielding. Therefore, only modest modifications of current methods of vessel flaw stress analysis will be suggested. On the other hand, more ductile, perhaps fully plastic, failures are characterized by significant amounts of stable flaw growth. Therefore, a more advanced (R-curve) stability concept will be suggested, especially for material property evaluation purposes. The new methodology can be considered as an extension of the existing Code methods, written in terms of J-Integral EPFM, for which LEFM is simply a special case.

Indeed, the only really new embellishment to be presented herein is the use of a J versus T diagram to assess crack instability. It is simply a new diagramatic representation of J-R curve material representation and applied J-T curves from established methods. It is proposed to clarify situations which will lead to crack instability, to simply delineated regions of rigorous applicability of the analytic concepts, to clearly demonstrate safety margins for approaching instability, and so forth. However, the use of J versus T diagrams involves no new assumptions; it is just a new representation method which clarifies many matters. One further result, which will be demonstrated, is that the limiting allowable J values suggested herein to avoid crack instability on the J versus T diagram have, so far, shown good correlation with Charpy upper shelf energies. This can be of great practical significance where only Charpy data are available.

THE PLANE-STRAIN J-INTEGRAL R CURVE

According to developments by Hutchinson[6] and Rice and Rosengren,[7] the value of the J-Integral (or $J_{applied}$) can be seen to be a parameter characterizing the intensity of the plastic stress-strain field surrounding the crack tip. Their results lead to the following form for the stress-strain field; that is, the HRR field

$$\sigma_{ij} = \sigma_0 \left(\frac{J}{r\varepsilon_0\sigma_0}\right)^{\frac{1}{n+1}} \bar{\Sigma}_{ij} (\theta, n)$$
$$\varepsilon_{ij} = \varepsilon_0 \left(\frac{J}{r\varepsilon_0\sigma_0}\right)^{\frac{n}{n+1}} \bar{E}_{ij} (\theta, n) \quad (1)$$

plus higher order terms (negligible near a crack tip). The coordinates r and θ are the usual cylindrical coordinates measured from the crack tip. The analysis was based on adopting a deformation theory of plasticity for a stress-strain curve whose latter portion (well beyond the elastic range) can be represented by a power law or

$$\frac{\varepsilon}{\varepsilon_0} = \left(\frac{\sigma}{\sigma_o}\right)^n \quad (2)$$

then $\bar{\Sigma}_{ij}$ and $\bar{E}_{ij}$ are particular specified functions for the distribution of stresses and strains surrounding the crack tip.

The above approach assumes that two different cracks in the same material will have identical stress-strain fields surrounding the crack tips if loaded to the same intensity, J. It follows that if the stress and strain fields for the two cracks are identical then what happens within them is identical, such as increments of extension, Δa of the tips of the cracks. Hence, it is argued that a plot of J versus Δa, the J-R curve, is a unique plot of a material's crack extension characteristics. Indeed, this is the very same argument upon which LEFM is based for K-controlled crack tip fields. Though the logical basis of the J-R curve is equivalent to that of LEFM, the assumptions, conditions, and limitations should be clearly specified because they are less familiar than those for LEFM. Unless otherwise specified, they are

(1) that conditions in the material's crack tip fracture process zone are plane-strain

(2) that conditions which disrupt the HRR field are avoided, such as avoiding concentrated slips direct from the crack tip to nearby boundaries or cross-slip (slip at 45° through the thickness)

(3) that crack growth does not disrupt the HRR fields

(4) that cleavage does not intercede on the J-R curve.

Indeed, J-R curves produced by the types of test conditions proposed by ASTM Committee E-24 for standards at least attempt to be sufficient to avoid conditions (1) and (2) as problems. Indeed, condition (4) is thought not to be a problem at temperatures exceeding 100°C above the transition temperature (beginning of upper shelf); but, more data on this point may be needed. Finally, (3) is not a problem under conditions proposed by Hutchinson[3] which are

$$\omega = \frac{dJ}{da}\ \frac{b}{J} >> 1$$

and (3)

$$\Delta a << b$$

Hutchinson[3] showed by differentiating (1), obtaining the increments of the strain, $d\varepsilon_{ij}$, that these increments $d\varepsilon_{ij}$ are sufficiently proportional to ε_{ij} to ensure appropriate use of deformation theory. The use of J itself here is also based on having conditions sufficiently appropriate for deformation theory. Hence equations (3) also ensure sufficient conditions for the definitions of J in its integral forms to follow. [It should be noted that sufficient conditions are distinct from necessary conditions, and therefore equations (3) may not always be required for appropriate use of J.]

Therefore, under the given conditions the applicability of "strict deformation theory" is appropriate, the conditions for so called "J-controlled crack growth" are met, and J may be defined with equal validly either by its contour integral or compliance counterparts which are, according to, Reference 8 (see also Reference 9 for details):

$$J = \int_{\Gamma} Wdy - T_i \frac{\partial u_i}{\partial x} ds$$

(Γ is any contour around the crack tip) (4)

or

$$J = -\int \frac{\partial P}{\partial a} d\sigma_p = \int \frac{\partial \sigma_p}{\partial a} dp$$

Consequently, the "plane strain J-R curve," as shall be adopted here, is assumed to be produced under appropriate conditions as discussed under the four conditions stated above. J should be measured by a method consistent with applying equations (4), including crack length change, Δa, corrections. The J-R curve is then a plot of J versus Δa points as loading progresses on a cracked sample of the material at a given temperature.

Further, the J-R curves available may not always have been produced under ideal conditions (often undersized test specimens). This will not rule out their use if they can be shown to be conservative. For example, slightly subsized samples and/or the use of side grooves with appropriate data reduction methods have been shown to give conservative J-R curves for bending-type tests. As used here, conservatism is taken with respect to safety when the test results are used to evaluate applications by the methods developed later in this report.

THE TEARING INSTABILITY CRITERION

In equations (1) above it was noted that J is the intensity of the crack tip stress and strain field. Moreover, with proportional straining as guaranteed by meeting the conditions of equations (3), it can be argued that appropriate use of "strict deformation theory" and "J-controlled crack growth" will result. Therefore, at least under these conditions, the second definition of J in equations (4) implies that

$$J_{applied} = \frac{dU}{da} \tag{5}$$

where U is pseudo-elastic energy per unit thickness stored (that is, for the nonlinear elastic analogue to an elastic-plastic material) by applying loads or deformation to the cracked body of interest. Regarding crack length change, da, as a displacement, $J_{applied}$ takes on the connotation of a generalized force and $J_{material}$ may be regarded as the material's resistance to that force.

Consequently, a statement of equilibrium with respect to crack extension is

$$J_{applied} = J_{material} \tag{6}$$

The stability of the equilibrium expressed by equation (6) can be found by examining the second derivative of system energy. Using equation (5), the stability criterion can be written

$$\frac{d^2U}{da^2} = \frac{dJ_{applied}}{da} \begin{matrix} < \\ = \\ > \end{matrix} \frac{dJ_{material}}{da}. \tag{7}$$

For convenience, the tearing modulus, T, is defined as

$$T = \frac{dJ}{da} \frac{E}{\sigma_o^2} \tag{8}$$

where E is elastic modulus and σ_o is flow stress. Then the stability criterion, equation (7), may be expressed in nondimensional terms by

$$T_{applied} \begin{matrix} < \\ = \\ > \end{matrix} T_{material} \begin{matrix} \text{(stable)} \\ \text{(indifferent)} \\ \text{(unstable)} \end{matrix} \tag{9}$$

Now, $J_{applied}$ may be found from the stress analysis solution for the cracked body, applying equations (4) to make the determination. Consequently, $J_{applied}$ will depend on applied loads, P, or deformations, Δ, and crack size, a; hence

$$J_{applied} = J_{applied}\ (P,\ a)$$

or

$$= J_{applied}\ (\Delta,\ a) \tag{10}$$

On the other hand, $J_{material}$ depends on the material's resistance or its J-R curve, which is a plot of J versus Δa, characterizing the material's resistance to crack extension. Consequently,

$$J_{material} = J_{material}\ (\Delta a) \tag{11}$$

Therefore, when derivatives $\frac{d}{da}$ are taken of equations (10) and (11) to form $T_{applied}$ and $T_{material}$ as indicated in equation (8), it should be noted that $T_{material}$ may be formed from the slope of the J-R curve, $\frac{dJ_{material}}{da}$, taken at a given level of J. That is to say,

$$T_{material} = T_{material}\ (J) \tag{12}$$

On the other hand,

$$\frac{dJ_{applied}}{da} = \frac{\partial J_{applied}}{\partial P} \cdot \left(\frac{\partial P}{\partial a}\right) + \frac{\partial J_{applied}}{\partial a}$$

or

$$= \frac{\partial J_{applied}}{\partial \Delta} \cdot \left(\frac{\partial \Delta}{\partial a}\right) + \frac{\partial J_{applied}}{\partial a} \tag{13}$$

where the partial derivatives of $J_{applied}$ on the right sides of equations (13) are found from $J_{applied}$ solutions in the form of equations (10). The other ( ) partial derivatives in equations (13) depend on the load application system compliance and must be evaluated accordingly. Furthermore, assuming that the quantities in equations (13) are properly evaluated, it is observed that

$$T_{applied} = T_{applied}\ (P,\ a)$$

or

$$= T_{applied}\ (\Delta,\ a) \tag{14}$$

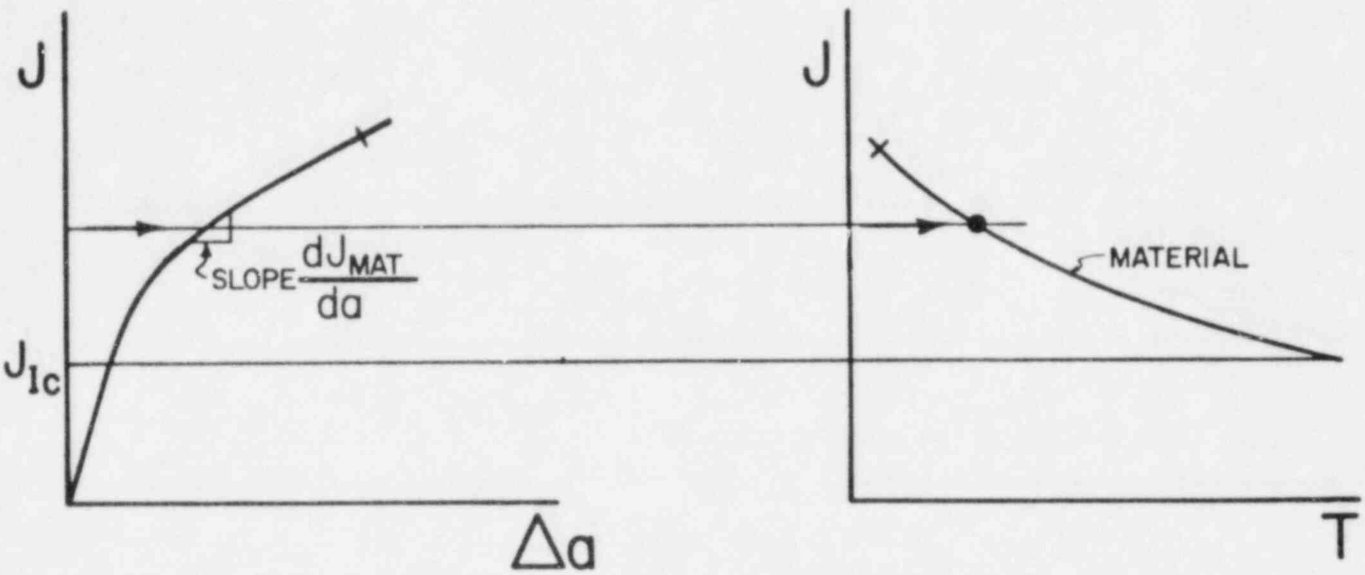

Figure 1 A material's J-R curve replotted on a J-T diagram

such as indicated by the arrow, the slope, $\frac{dJ_{material}}{da}$, may be determined. As defined by equation (8), then

$$T_{material} = \frac{dJ_{material}}{da} \frac{E}{\sigma_0^2} \tag{17}$$

which establishes a point on the J versus T diagram on the right in Figure 1. Repeating the procedure at various J values will result in the J versus T material curve. Note that below J_{Ic} no crack extension takes place so $T_{material}$ is very large (that is, off scale). In this way, J-R curves can be transformed directly into J versus T-mat. curves.

In a typical J-R test, the remaining uncracked ligament, b, is the proper dimension to determine ω as defined by equations (3).

Therefore, dividing $J_{material}$ by $T_{material}$

$$\frac{J}{T}_{material} = \frac{J\,\sigma_o^2}{\frac{dJ}{da}\,E} = \frac{\sigma_o^2\, b}{E\,\omega} \tag{18}$$

Consider the conditions of assured validity, equations (3). As shown on Figure 1, a crack extension limit ($\Delta a << b$) may be placed on the R curve, with a corresponding mark at the same J level on the J versus T-mat. curve. Another limit (from equations (3)) can be represented in Figure 2 by a line of slope $\frac{\sigma_o^2 b}{E\,\omega}$

representing equation (18). The actual material properties (σ_o and E), specimen size (b), and smallest acceptable ω (perhaps 5 or smaller) determine the slope and, therefore, the intersection with, and the ω limit of, the materials curve. Therefore, the J versus T-mat. curve may be doubtful above the lower of these two limits. (It is presumed that all other J-R curve test requirements and practices are met satisfactorily.)

All J versus T material curves which have been plotted to date have shown concave upward behavior. Physical reasons why this should be observed will be omitted here. Accepting this empirically observed behavior, the material curve, at least from below the limit marks, could be extrapolated upward as a straight line extension of the valid curve to determine a safe J versus T loading region as shown on Figure 3. That is to say that if a cracked sample of the same material is loaded to a certain J-level, and the applied ($J_{applied}$, $T_{applied}$) point is in the "safe region" as shown in Figure 3, then for that J-level, $T_{applied} < T_{material}$ and the crack is stable according to equation (9).

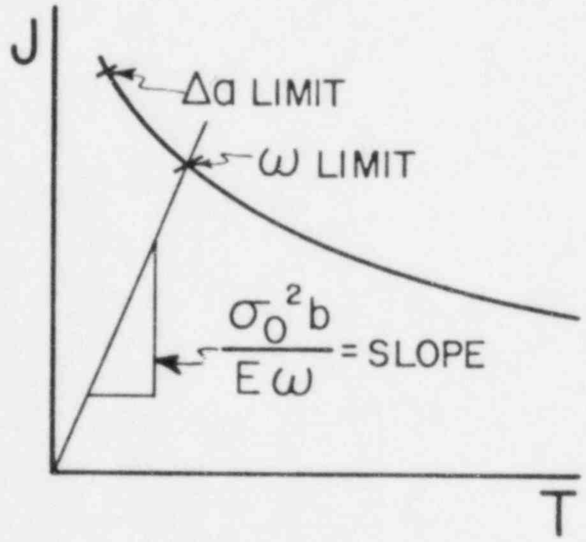

Figure 2 Assured validity limits noted on a J-T diagram

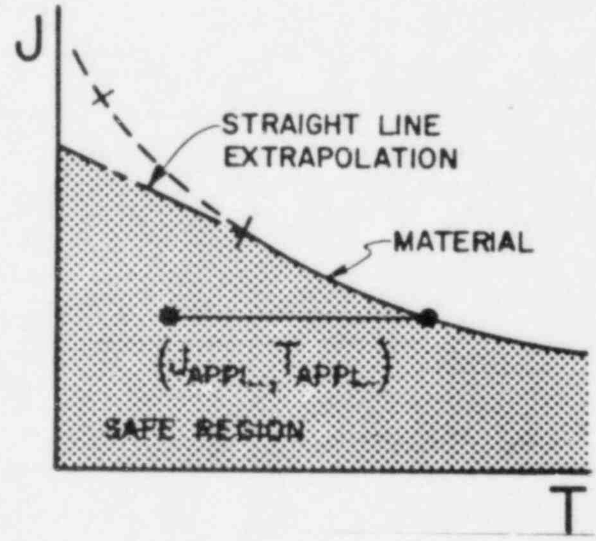

Figure 3 The safe region (J_{appl}, T_{appl}) for a given material

It remains to determine the trace of the ($J_{applied}$, $T_{applied}$) points for $T_{applied}$ (J)-curve, as loading or J increases, starting with no load. However, it is sufficient to observe that for the applications to be considered here*, the $T_{applied}$ curves always increase monotonically with J whereas the $T_{material}$ curves decrease monotonically with J, so the intersection of the two curves uniquely indicates the onset of instability; that is, no prior instabilities (intersections) can occur. This is illustrated on Figure 4.

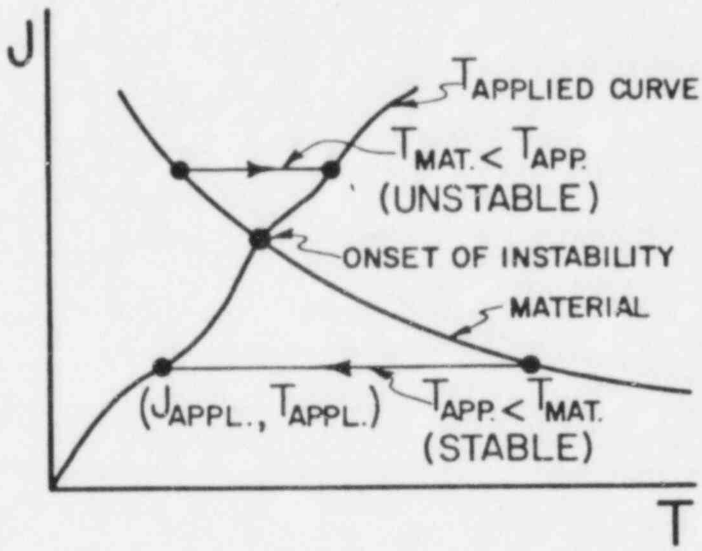

Figure 4 A schematic T-applied curve extending to instability

Analysis of typical $T_{applied}$ curves for the applications of interest will follow to demonstrate the monotonic increasing J versus $T_{applied}$ behavior. On the other hand, there are other applications such as testing for which always stable conditions are sought (in bending where $T_{applied}$ = negative). These are treated in earlier studies[2] sufficiently for the objectives of this current work. Nevertheless, it is noted and the reader is warned that other relevant considerations must be made where widely different loading conditions and crack configurations exist, such as plastic bending of nuclear piping with through-wall cracks. However, for the normal conditions and postulated flaws for pressure vessels, the J versus $T_{applied}$ behavior will follow a consistent pattern, as will be shown.

*Cracks in pressure vessel walls primarily loaded with internal pressure are considered here.

ANALYSIS OF J VERSUS T APPLIED CURVES FOR THROUGH CRACKS IN PRESSURE VESSEL WALLS

Under the actual pressures expected in nuclear pressure vessels, the shell stresses remain linear elastic and LEFM conditions apply. At a temperature high enough to be well into the Charpy upper shelf region and for flaw sizes of interest, it may take stresses approaching yield or higher to cause actual crack instabilities. Moreover, in assessing measured crack instabilities in model or full scale vessel tests, the necessary pressures resulted in stresses near or exceeding the yield of the material. Therefore, along with the previously developed J versus T diagram, stability analysis, and material characterization, it is necessary to develop analytical equations for $J_{applied}$ and $T_{applied}$ which are accurate when applied in the LEFM range and which also can be applied in the range where stresses exceed the yield strength. Thus factors of safety and/or results of vessel tests may be assessed at least approximately.

A. <u>Linear-Elastic Format</u>

In the linear-elastic range it is noted that

$$J = \frac{K^2}{E} \tag{19}$$

where for a cylindrical shell of radius, R, and thickness, t, with a through crack of length, 2a, the applied stress intensity factor, K, may be written

$$K = \sigma \sqrt{\pi a} \cdot Y(\lambda) \tag{20}$$

where $\lambda = a/\sqrt{Rt}$, and Y is a geometrical correction factor for the effect of shell curvature and bending. Substituting equation (20) into (19) and rearranging leads to a convenient form.

$$J_{applied} = \frac{\sigma_o{}^2 a}{E} \left\{\frac{\pi\sigma^2}{\sigma_o{}^2}\right\} [Y^2] \tag{21}$$

where we define

{ } = the stress bracket

[ ] = the geometry bracket

for the purposes to follow. For examining crack stability under constant pressure or load (that is, σ constant), the first form of equation (13) applies with $\partial P/\partial a = 0$; hence, following the definition of equation (8)

$$T_{applied} = \frac{dJ_{applied}}{da}\frac{E}{\sigma_o^2} = \frac{\partial J_{applied}}{\partial a}\frac{E}{\sigma_o^2} \tag{22}$$

putting equation (21) into (22) leads to

$$T_{applied} = \frac{\pi\sigma^2}{\sigma_o^2} \cdot [Y^2 \times 2\lambda Y \cdot Y'] \tag{23}$$

which contains the same stress bracket as equation (21) but a new geometry bracket. To identify the implied $J_{applied}$ versus $T_{applied}$ curve on a J versus T diagram by eliminating load or σ, simply divide equation (21) by (23) to obtain

$$\frac{J_{applied}}{T_{applied}} = \frac{\sigma_o^2 a}{E} \cdot \left[\frac{1}{1 + 2\lambda Y'/_Y}\right] \tag{24}$$

For constant crack size, a, and for a given material, the ratio of $J_{applied}$ to $T_{applied}$ is a constant according to equation (24), which can be represented as a straight line through the origin on a J versus T diagram as in Figure 5.

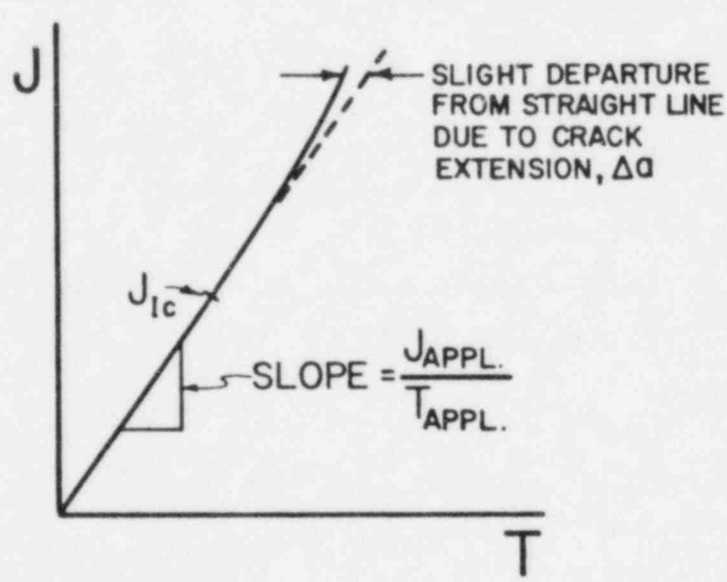

Figure 5 A typical J versus T applied curve, almost straight

As loading occurs (that is, stress σ is applied) from equation (21), $J_{applied}$ starts from zero (the origin of Figure 3) and proceeds to increase with the square of the applied stress. If J exceeds J_{Ic} crack extension, Δa (actual) begins to occur so the trace of $J_{applied}$ versus $T_{applied}$ would depart slightly from a straight line. But the crack length changes prior to the onset of instability are likely to be small in heavy sections so this slight departure will be neglected for the moment.

It remains to show how the $J_{applied}$ versus $T_{applied}$ curve behaves as stresses exceed the range of applicability of LEFM. But first it is relevant to establish values for the geometry brackets as given in equations (21), (23), and (24).

B. Shell Correction Factors or Geometry Brackets for Through Cracks in Cylindrical Shells With Internal Pressure

The shell correction factors for longitudinal through cracks in shells as developed first by Folias[10] and modified by Erdogan and Kibler[11] and verified by Krenk[12] are perhaps most conveniently shown in Rooke and Cartwright's work.[13] For the longitudinal crack, they can be empirically expressed over the range of interest by the approximations (± 1%):

$$Y = (1 + 1.25\lambda^2)^{\frac{1}{2}} \text{ for } (0 \leq \lambda \leq 1)$$

$$= (0.6 + 0.9\lambda) \text{ for } (1 \leq \lambda \leq 5)$$

where, as before (25)

$$\lambda = \frac{a}{\sqrt{Rt}}$$

Similar expressions may be developed for circumferential cracks (again see Reference 13), but are of lesser interest since the applied longitudinal stresses are a factor of 2 less than the hoop stresses, which favors cracking.

Using expressions such as equations (25) or curves from Reference 13, the geometry brackets required in equations (21), (23), and (24) have been computed and are given here graphically in Figures 6, 7, 8, and 9 for both longitudinal and circumferential through cracks.

In the following discussion, it will be of special interest to note that the geometry bracket associated with equation (24) (dashed curves on Figures 6 to 9) is always a number smaller than 1 and greater than 1/3. Indeed for most vessels, $R/t \cong 10$ and the usual leak-before-break assumption of $a = t$ gives $\lambda \cong 0.31$ and the [ ] is between 1 and 0.8; that is, always nearly 1 in equation (24).

C. Plastic Zone Corrected LEFM Conditions

Historically the first attempts to extend LEFM toward the elastic-plastic range included correcting the crack length for the plastic zone at the crack tip to obtain an effective crack size, a_{eff}; that is:

$$a_{eff} = a + r_y$$

where

$$r_y = \frac{1}{\beta\pi}\left(\frac{K^2}{\sigma_o^{\,2}}\right) = \frac{JE}{\beta\pi_o^{\,2}} \tag{26}$$

where $\beta \cong 2$ (for plane stress)

$\cong 6$ (for plane strain)

In applying the plastic zone correction to equation (21), for example, the crack size, a, might be replaced by $a_{eff.}$, both where it appears explicitly and in Y. However, its use here shall be restricted to relatively low nominal stress levels (for example, $\frac{\sigma}{\sigma_o} < \frac{2}{3}$, so $r_y \ll a$) and its effect on the value of the geometry bracket $[Y^2]$ and others will be small enough to be neglected. Correcting only the explicit appearance of a in equation (21) and rearranging gives the result

$$J_{applied} = \frac{\sigma_o^{\,2} a}{E} \left\{ \frac{\pi\left(\frac{\sigma}{\sigma_o}\right)^2}{1 - \frac{Y^2}{\beta}\left(\frac{\sigma}{\sigma_o}\right)^2} \right\} [Y^2]. \tag{27}$$

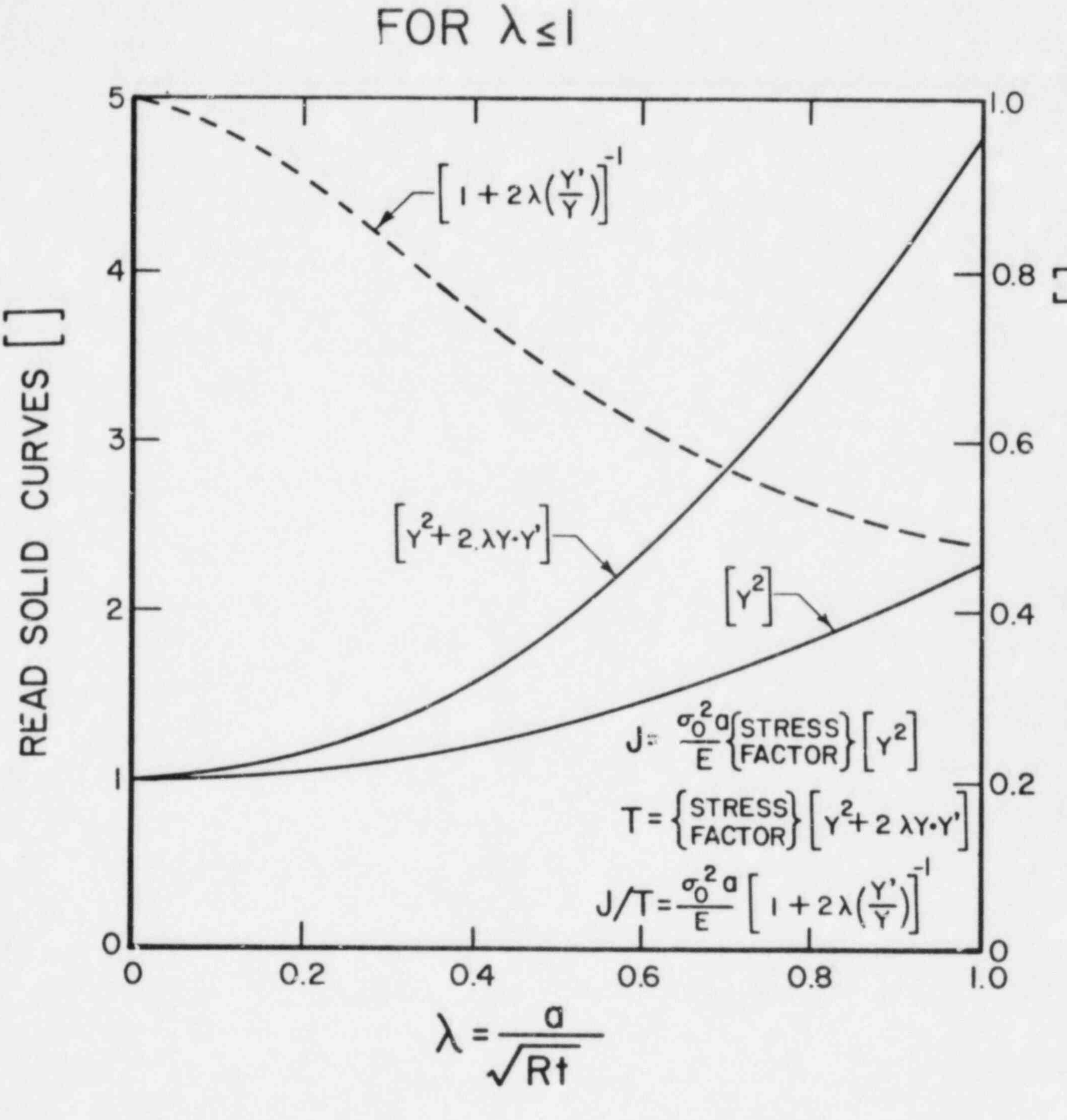

Figure 6 Shell correction factors for longitudinal cracks in cylinders (for low λ)

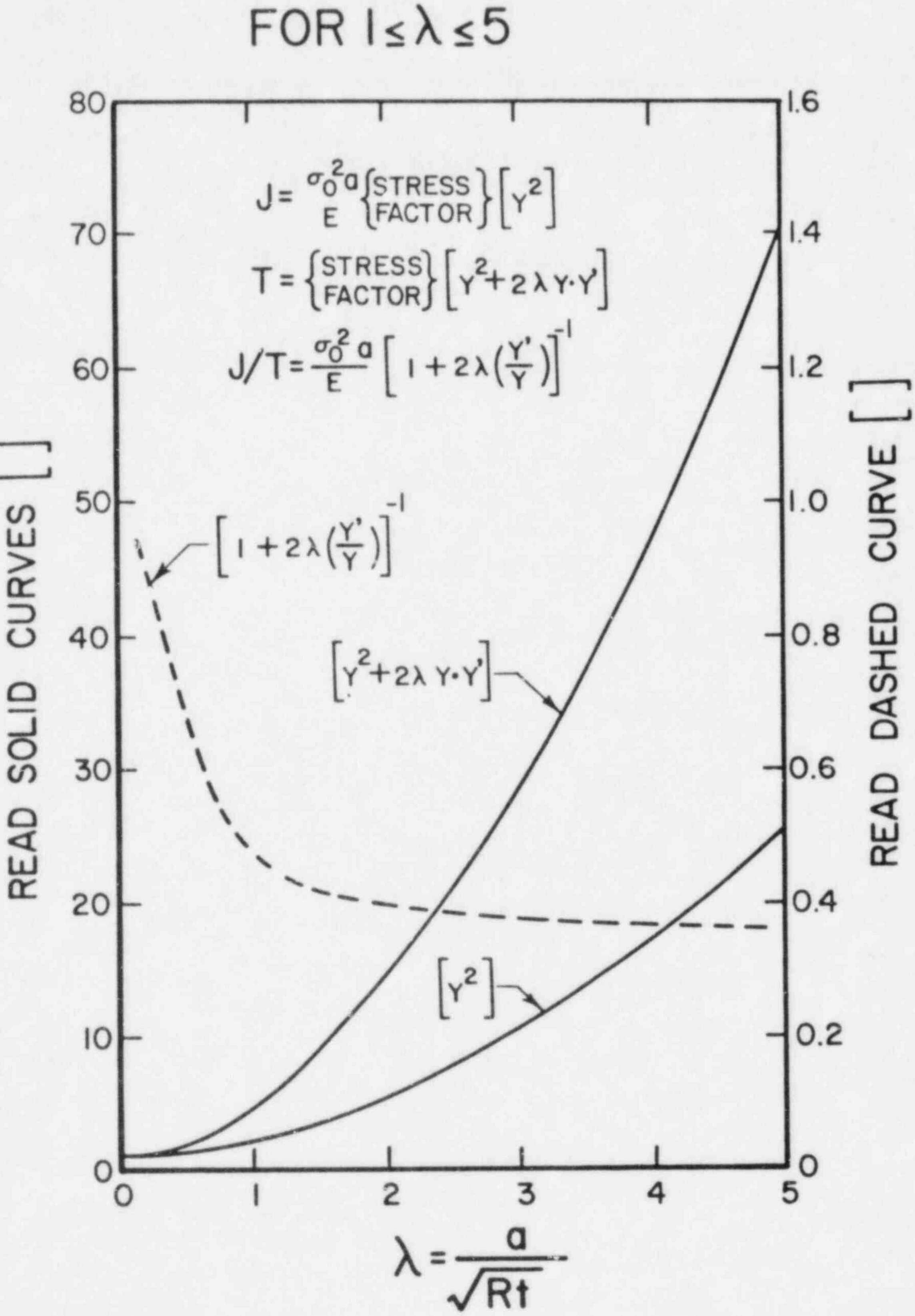

Figure 7 Shell correction factors for longitudinal cracks in cylinders (for high λ but with $\sigma/\sigma_0 \leq 0.67$)

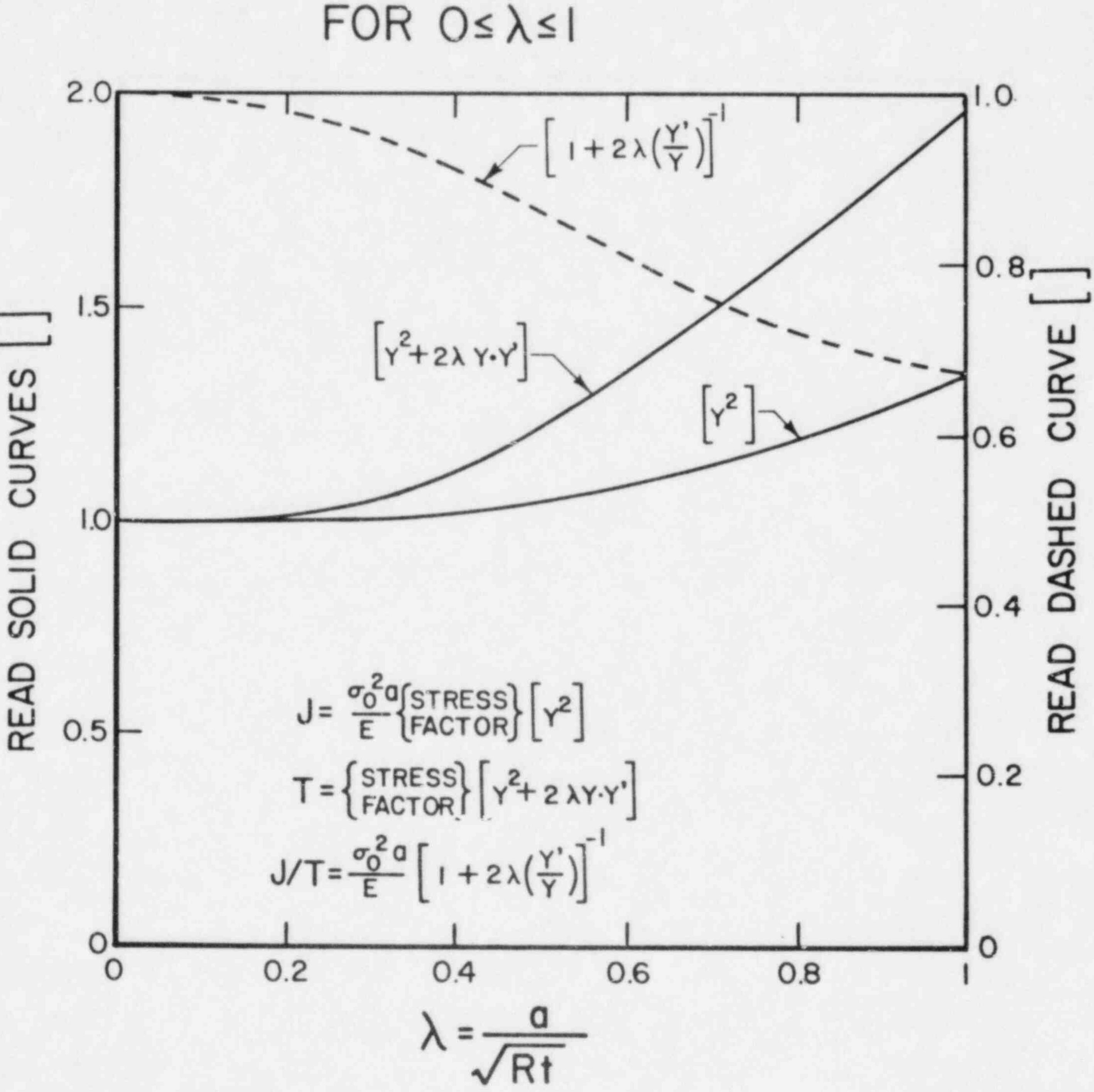

Figure 8 Shell correction factors for circumferential cracks in cylinders (for low λ)

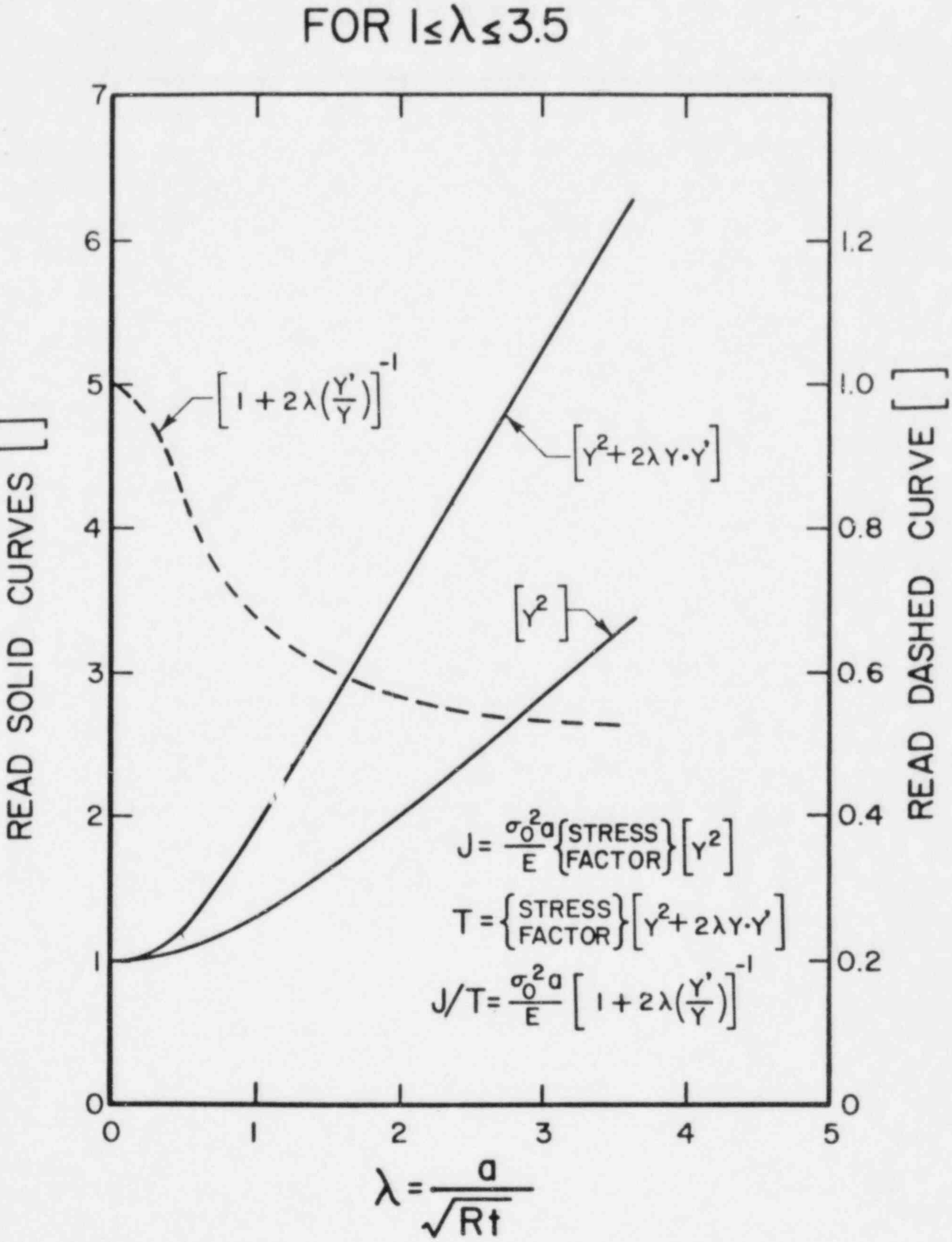

Figure 9 Shell correction factors for circumferential cracks in cylinders (for high λ but with $\sigma/\sigma_0 \leq 0.67$)

For through cracks in nuclear vessels, where instability is approached at stress levels of 2/3 yield or less, the crack tip plastic zone stress state will be closer to plane strain than plane stress. Hence, to simplify the stress bracket in equation (27), taking $Y^2 \cong 1$ (but using the actual values from curves) and $\beta = 2$ (plane stress thus conservative), a conservative estimate of $J_{applied}$ is achieved:

$$J_{applied} = \frac{\sigma_o^2 a}{E} \left\{ \frac{\pi \left(\frac{\sigma}{\sigma_o}\right)^2}{1 - \frac{1}{2}\left(\frac{\sigma}{\sigma_o}\right)^2} \right\} [Y^2]. \tag{28}$$

Indeed, most often the 1/2 in the stress bracket might be too conservative, but it can be no less than 1/6. For the range of interest, Figure 10 shows a plot of these extremes for the stress bracket. Using the conservative value 1/2 also compensates for the slight underestimate of the geometry bracket, $[Y^2]$, by neglecting the plastic zone correction in it.

Finally, it is noted that the simplifying assumptions leading to equation (28) not only result in a good (perhaps slightly conservative) approximation for $J_{applied}$, but most importantly result in an especially convenient format. The stress bracket and geometry brackets in equation (28) completely separate the stress and geometry effects on $J_{applied}$ into independent factors. Because of the separation and, operating on equation (28), following the analysis represented by the sequence: equation (21) to equations (23) and (24), the results are:

$$T_{applied} = \left\{ \frac{\pi \left(\frac{\sigma}{\sigma_o}\right)^2}{1 - \frac{1}{2}\left(\frac{\sigma}{\sigma_o}\right)^2} \right\} [Y^2 + 2\lambda YY'], \tag{29}$$

and

$$\frac{J_{applied}}{T_{applied}} = \frac{\sigma_o^2 a}{E} \left[\frac{1}{1 + 2\lambda Y'/_Y} \right] \tag{30}$$

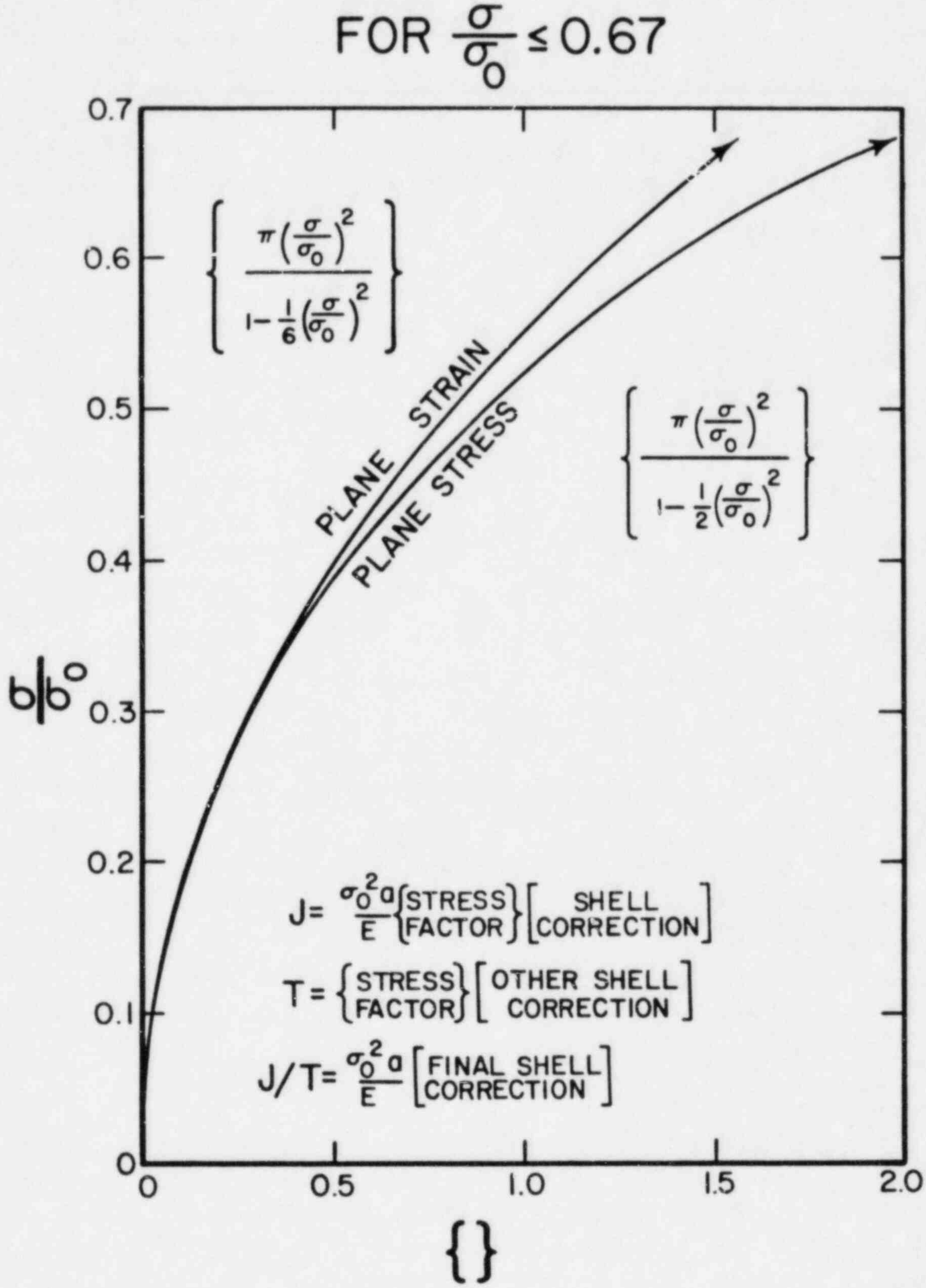

Figure 10 Stress correction factors for J and T for low stress ($\sigma/\sigma \leq 0.67$)

It should be noted that the final result, equation (30), is identical to equation (24), which implies that on the J versus T diagram, Figure 5, the same $J_{applied}$ - $T_{applied}$ trace or loading line is followed, whether the plastic zone correction is used or not! Consequently, as discussed earlier, the loading line is a straight line through the origin of the J versus T diagram of a slope given by

$$\underline{\text{slope}} = \frac{J_{applied}}{T_{applied}} = \frac{\sigma_o{}^2 a}{E} \text{ [order of 1]} \cong \frac{\sigma_o{}^2 a}{E} \tag{31}$$

D. <u>A Note on Further Extrapolation of the Stress Bracket</u>

The analysis of actual nuclear vessels at nominal stress levels above 2/3 yield is not realistically associated with any known operating or even faulted conditions. However, for the purpose of comparison of analytical methods with test results from model vessel tests pressurized to crack instability, extrapolation of the above methods to obtain fair approximations is relevant.

Moreover, at stress levels higher than 2/3 yield, interest becomes centered on rather short through cracks, $a \ll t$, so that $\lambda \ll 1$ and the geometry correction effects become small. Under such conditions, the separation as in equation (28) to independent stress brackets and geometry brackets is no less justified; thus it need not be discussed further here. For the stress bracket functions derived below, it must be noted that they should be applied only for low λ ($\lambda < 1$) so Figures 6 and 8 will be relevant but Figures 7 and 9 should be excluded.

E. <u>The Strip Yield Model Stress Bracket</u>

Using the so-called Dugdale strip yield model to develop the stress bracket, the development of the function follows equations (21), (23), and (24), or equally well by equations (28), (29), and (30), repeated here for emphasis:

$$J_{applied} = \frac{\sigma_o^2 a}{E} \left\{ \right\} [Y^2]$$

and

$$T_{applied} = \left\{ \right\} [Y^2 + 2\lambda YY']$$

and (32)

$$\frac{J_{applied}}{T_{applied}} = \frac{\sigma_o^2 a}{E} \left[\frac{1}{1 + 2\lambda Y'/Y} \right]$$

where

$\left\{ \right\}$ = the stress bracket

From the solution for the strip yield model for a center through-cracked plate (for example, see Reference 14) and comparing results with the first of equations (32), the stress bracket <u>for strip yielding</u> is

$$\left\{ \right\} = \left\{ \left(\frac{8}{\pi\gamma}\right) \ln \sec \left(\frac{\pi}{2} \frac{\sigma}{\sigma_o}\right) \right\} \quad (33)$$

where

0.7 (for plane strain) $\leq \gamma \leq 1$ (for plane stress).

This stress bracket might be used for stress levels from 2/3 yield up to (but not including) the yield strength (it assumes elastic perfectly plastic non-hardening material). It is appropriate to go on to hardening solutions for extrapolation of the stress bracket for stresses at or above the yield strength.

F. The Power Hardening Stress Bracket

For a power hardening approximation of a material's stress strain curve by

$$\frac{\varepsilon}{\varepsilon_0} = \bar{\alpha}\left(\frac{\sigma}{\sigma_o}\right)^n \tag{34}$$

the numerical solutions for center-cracked plates under both plane stress and plane strain have been presented by Hutchinson and coworkers.[15] Their results were compiled and applied to develop tearing instability parameters by Zahoor[16] and tabulated by Tada.[17] Taking their plane stress results in the same form as the first of equations (32), the stress bracket becomes

$$\left\{\right\} = \left\{\bar{\alpha} f^* \left(\frac{\sigma}{\sigma_o}\right)^{n+1}\right\} \tag{35}$$

where

$$\begin{aligned} f^* &= \pi \ (n = 1) & &= 9.2 \ (n = 7) \\ &= 5.5 \ (n = 3) & &\text{and so forth} \\ &= 7.5 \ (n = 5) & & \end{aligned}$$

The power hardening model, equation (34), is a fair approximation only above yield for nominal stresses. Therefore, its use is limited. However, if only the above yield range is of interest in certain applications, some further simplifications may be invoked. Dividing equation (35) by (34) and rearranging:

$$\left\{\right\} = \left\{f^* \left(\frac{\sigma}{\sigma_o}\right) \left(\frac{\varepsilon}{\varepsilon_o}\right)\right\} \tag{36}$$

Above yield the stress is always near the yield stress $\sigma \cong \sigma_0$ (or equation (34) can be adjusted). Hence, in the above-yield range the stress bracket is almost proportional to the strain, or more properly the stress times the strain. Substituting equation (36) into the first of equations (32)

$$J_{applied} = f^* \sigma\varepsilon a[Y^2] \tag{37}$$

Noting that in this relationship $J_{applied}$ varies approximately linearly with nominal stress, σ, with nominal strain, ε, and with crack size, a, is of considerable assistance in intuitively understanding the roles of loading deformation and geometry (crack size) as variables affecting $J_{applied}$.

However, the simple power hardening model of a material's stress-strain curve, equation (34), is inadequate to represent the detailed behavior of both the elastic range and the hardening range. A better representation is found through the Ramberg-Osgood approximation.

G. The Ramberg-Osgood Stress Bracket

The Ramberg-Osgood representation of a material's stress-strain behavior is

$$\frac{\varepsilon}{\varepsilon_o} = \frac{\sigma}{\sigma_o} + \bar{\bar{\alpha}} \left(\frac{\sigma}{\sigma_o}\right)^n \tag{38}$$

Again from Hutchinson's results[15] as compiled by others,[16,17] comparing terms in the same form as the first of equations (32), the stress bracket may be written:

$$\left\{\right\} = \left\{\Psi^* \left(\frac{\sigma}{\sigma_o}\right)^2 + \bar{\bar{\alpha}} \;\; G^* \left(\frac{\sigma}{\sigma_o}\right)^{n+1}\right\} \tag{39}$$

The parameters Ψ^* and G^* vary in a complex way with $\bar{\bar{\alpha}}$ and n which can be determined from analysis in References 16 and 17. The limiting case for elastic material, $\bar{\bar{\alpha}} = 0$ is $\Psi^* = \pi(G^* \neq \infty,\ n \neq \infty)$. Thus equation (39) is seen to reduce to a form proper for insertion in equation (21). At the other limit, with the stress above yield, $\sigma > \sigma_o$, the Ψ^* term is negligible and then $\bar{\bar{\alpha}}\ G^* \cong \alpha f_,^*$ which produces agreement with equation (35).

It would be cumbersome to present stress-strain curve fitting considerations using equation (38), as well as corresponding determinations of Ψ^* and G^* for all materials here. More to the point is to consider a typical material, A533B, at 93°C, for which Shih[18] obtained the following curve-fitting results:

σ_o = 60 ksi
E = 29 x 10^3 ksi
$\bar{\bar{\alpha}}$ = 1.115
n = 9.7

Following References 16 and 17 for plane stress and using these results, one obtains

$$\psi^* = 4.3$$
$$\bar{\bar{\alpha}}G^* = 11.8$$

which, when substituted in equation (39), gives

$$\left\{\ \right\} = \left\{4.3 \ \left(\frac{\sigma}{\sigma_o}\right)^2 + 11.8 \left(\frac{\sigma}{\sigma_o}\right)^{10.7}\right\} \qquad (40)$$

for a typical nuclear vessel material. Plotting the stress bracket, equation (40), for $\frac{2}{3} < \frac{\sigma}{\sigma_o} < 1$, and fairing it into the stress bracket from equation (28) for $\frac{\sigma}{\sigma_o} < \frac{2}{3}$ resulted in the curve of Figure 11. Again, the reader is reminded that the curve in Figure 11 is appropriate only for situations where $\lambda << 1$, so the elastically determined geometry brackets must not be used inappropriately in the fully plastic range.

SUMMARY ON THROUGH-CRACK ANALYSIS

In summary, a method has been developed to analyze through cracks in nuclear pressure vessels to determine $J_{applied}$, $T_{applied}$ and $J_{applied}/T_{applied}$. Neglecting both a plastic zone correction to the geometry factor and geometry correction to the stress factor forced a separation of effects which was compensated by developing stress factors for plane stress (conservative). For application at stress levels below 2/3 yield <u>or</u> at low values of λ ($\lambda << 1$), the method is accurate and slightly conservative. At stresses above 2/3 yield <u>or</u> with high λ ($\lambda > 1$), but not both, the method will give good approximations. This permits comparison of analytical predictions with many test results. (The method is not intended to treat long through cracks ($\lambda > 1$), concurrent with high nominal stresses (approaching or above yield), but this combination is never encountered in nuclear vessel analysis.)

The resulting equations for all cases were reduced to equations (32). The geometry brackets were given in Figures 6, 7, 8, and 9, and the stress brackets in Figure 11 (and 10).

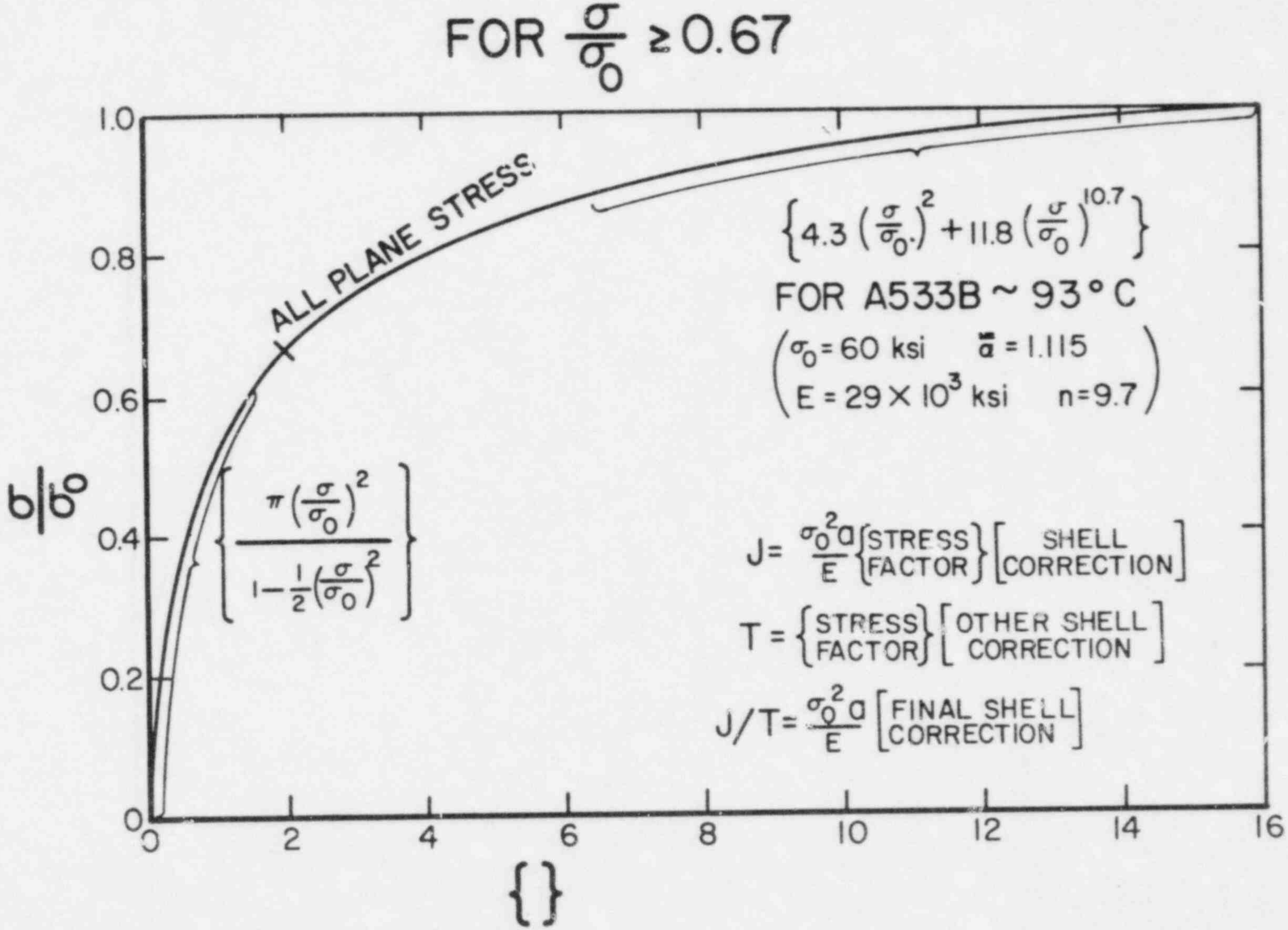

Figure 11 Stress correction factors for J and T extended to high stress

Finally, the loading line on a J versus T diagram for the trace of $J_{applied}$ versus $T_{applied}$ is effectively a straight line through the origin of slope equal to

$\frac{\sigma_o^2 a}{E}$ times a factor which ranges from 0.5 to 1. This result is independent of the stress bracket model employed.

SURFACE FLAW ANALYSIS

For a surface flaw of depth, a, and length, 2c, in a vessel wall of thickeness, t, the form of the elastic solution for K is often given as

$$K = \frac{\sigma\sqrt{\pi a}}{\phi_o\left(\frac{a}{c}\right)} f\left(\frac{a}{c}\right) \cdot g\left(\frac{a}{t}\right) \quad (41)$$

where ϕ_o is the elliptical shape factor as computed from the complete elliptic integral of the second kind [19]:

$$\phi_o = \int_o^{\pi/2} \left[1 - \frac{c^2 - a^2}{c^2} \sin^2 \theta\right]^{\frac{1}{2}} d\theta$$

and where;

$f\left(\frac{a}{c}\right)$ = a front surface correction factor;

$g\left(\frac{a}{t}\right)$ = a back surface correction factor.

One may combine ϕ_o and f into F by

$$F\left(\frac{a}{c}\right) = \left(\frac{f\left(\frac{a}{c}\right)}{\phi_o\left(\frac{a}{c}\right)}\right)^2$$

A. LEFM Surface Flaw Equations

Writing $J_{applied}$ directly from the above

$$J_{applied} = \frac{K^2}{E} = \frac{\sigma_o^{\,2}a}{E} \left\{\pi\frac{\sigma^2}{\sigma_o^{\,2}}\right\} F(\tfrac{a}{c})\cdot G(\tfrac{a}{t}) \qquad (42)$$

where $G(\frac{a}{t}) = [(\frac{a}{t})]^2$ and $G'(\frac{a}{t}) = \frac{dG}{d(\frac{a}{t})}$, and so forth.

Differentiating under constant pressure stress, σ, as before to obtain $T_{applied}$ gives

$$T_{applied} = \left\{\ \right\} F(\tfrac{a}{c}) \left[G(\tfrac{a}{c}) + \tfrac{a}{t}\, G'\ (\tfrac{a}{t})\right] \qquad (43)$$

where the derivatives of F are neglected since they are slightly negative for increasing a compared to c. This gives a conservative result for $T_{applied}$. Proceeding as before, dividing equation (42) by equation (43)

$$\frac{J_{applied}}{T_{applied}} = \frac{\sigma_o^{\,2}a}{E} \cdot \left[\frac{1}{1 + \frac{a}{t} \cdot \frac{G'}{G}}\right] \qquad (44)$$

Notice that the form of this result is identical to those for through flaws. Indeed, taking G to be the often employed "secant correction"[14] or

$$G(\tfrac{a}{t}) = \sec \frac{\pi a}{2t}$$

the geometry bracket in equation (44) is given by:

$$[\ \ ] = \left[\frac{1}{1 + \frac{\pi a}{2t}\tan\frac{\pi a}{2t}}\right] \qquad (45)$$

which for $0 \leq \frac{a}{t} \leq 1/2$ takes on values which range from 1 to 0.57. Hence, as before, the geometry bracket in equation (44) is slightly less than but nearly equal to 1 for cases of interest. This result is also independent of adjustment to the stress bracket, does not enter equation (44), and is independent of the crack shape aspect ratio (that is, does not include the function $F(\frac{a}{c})$. This elastic analysis should be tentatively restricted to avoid yielding of the uncracked remaining ligament, t-a, behind the crack. It is certainly acceptable if

$$\frac{\sigma}{\sigma_o} < (1 - \tfrac{a}{t}).$$

For additional reasons associated with correctness of the form of the above elastic analysis, it is prudent to restrict its use to a/t values equal to or less than 1/2.

B. The Surface Flaw With Yielding Remaining Ligament

Consider the case where a/t is greater than 1/2 and where

$$1 > \frac{\sigma}{\sigma_o} > (1 - \frac{a}{t}).$$

The ligament behind the crack will surely yield, but the uncracked regions of the vessel wall will be elastic. Following the analysis of the yielded ligament behind a surface flaw as in Reference 2 (that is, as an elastic through crack with the remaining ligament supplying distributed closing forces equal to the flow stress over the net section area), the displacement at the center of the crack is taken to be equal to the crack opening stretch, δ, or

$$\delta = \gamma \frac{J}{\sigma_o} = \frac{2\sigma_{eff}\ (2c)}{E} = \frac{4c}{E}\ [\sigma - \sigma_o\ (1 - \frac{a}{t})]. \quad (46)$$

Solving for $J_{applied}$ gives

$$J_{applied} = \frac{\sigma_o^{\ 2}a}{E}\ (\frac{4c}{\gamma a})\ (\frac{\sigma}{\sigma_o} - 1 + \frac{a}{t}). \quad (47)$$

Note that stress and geometry effects are necessarily mixed here. However, $T_{applied}$ can be computed again by differentiating with constant nominal pressure stress or:

$$T_{applied} = \frac{4c}{\gamma t}\ . \quad (48)$$

This result was presented in Reference 2. Continuing, we divide equation (47) by (48), resulting in

$$\frac{J_{applied}}{T_{applied}} = \frac{\sigma_o^{\ 2}a}{E}\ (\frac{t}{a})\ \ (\frac{\sigma}{\sigma_o} - 1 + \frac{a}{t}). \quad (49)$$

Under the conditions stated above, the final parenthesis in equation (49) is positive but less than a/t. Therefore, the product of the final two parentheses is less than, but nearly equal to, 1. Hence, comparing the form of equation (49) to equation (44) and earlier results (such as the last of equation (32)) shows that all can be described by

$$\frac{J_{applied}}{T_{applied}} = \frac{\sigma_o{}^2 a}{E} [\sim 0.5 \text{ to } 1]. \tag{50}$$

Figure 12 shows the values of the [ ] factor from equation (49) and equation (45) faired together from high a/t to low a/t respectively, consistent with the limitations of these equations.

The discussion has established that equation (50) applies to surface flaws, as well as through flaws. However, the analysis is recommended currently only for reasonably shallow surface flaws, that is, $a/t \leq 1/2$. Moreover, for good precision over a wide range of stress levels, the stress bracket should be further developed.

C. The Stress Bracket for the Surface Flaw

The geometry correction for the surface flaw (that is, $F(\frac{a}{c})$ and $G(\frac{a}{t})$ in equation (42)) is adequately represented by the curves for M and Q in Appendix A of Section XI of the Nuclear Pressure Vessel Code. The curve for Q where $\frac{\sigma}{\sigma_o} = 0$, that is, uncorrected for plastic zone effects, is most appropriate here (not overly conservative), since the plastic zone correction and other higher stress level effects shall be treated by modifying the stress bracket. The geometry correction suggested here is M^2/Q.

First, consider a plastic zone correction for the surface flaw formula, equation (42). As noted previously, since $F(\frac{a}{c})$ diminishes with increasing a, its effect somewhat cancels the increase in $G(\frac{a}{t})$. Thus a small plastic zone correction will have little effect on the values of the combined geometry correction terms. On the other hand, the explicit appearance of a in equation (42) can be plastic

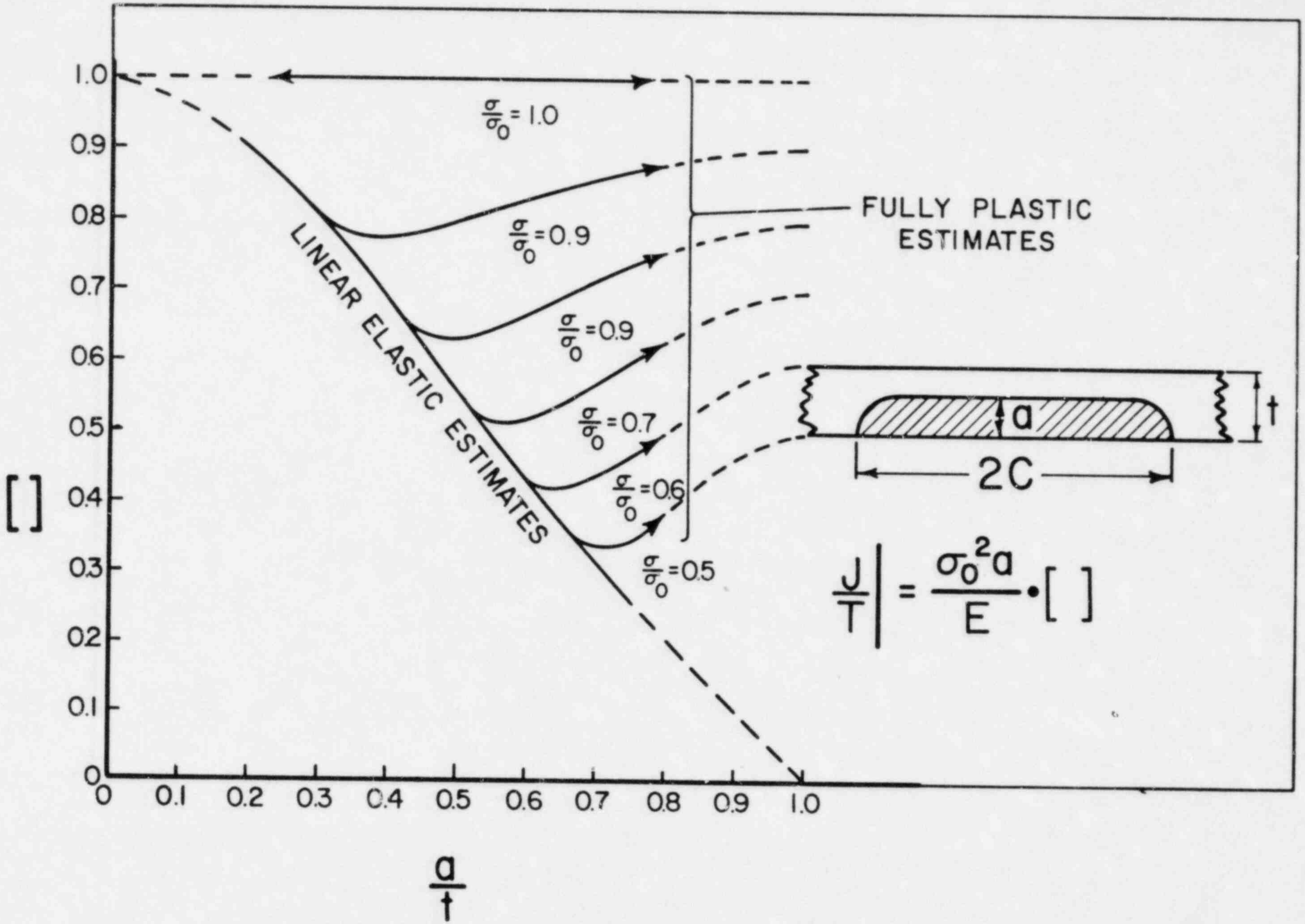

Figure 12 The J/T geometry correction [ ] for surface flaws for elastic and plastic ligaments

zone corrected, using equation (26) for plane strain. Up to stress levels of 2/3 of yield, this can be accomplished by adopting the plane-strain stress bracket correction as plotted on Figure 10, or

$$\left\{ \quad \right\} = \left\{ \frac{\pi \left(\frac{\sigma}{\sigma_o}\right)^2}{1 - \frac{1}{6}\left(\frac{\sigma}{\sigma_o}\right)^2} \right\}. \tag{51}$$

The (1/6) coefficient in the stress bracket could be corrected for geometry effects, but, in the stress range of applicability and for $a/t \leq 1/2$, this correction is small and applies to a term of small influence; thus, it well may be neglected, especially since it is a greater convenience to avoid mixing stress and geometry factors.

As was done before for through-thickness flaws, the stress bracket correction approach may be accomplished most appropriately for higher stress levels by employing the hardening results of Hutchinson and coworkers.[15] In this case, plane strain Ramberg-Osgood hardening analysis was employed. Indeed, for very shallow ($a/t \ll \frac{1}{2}$) but long ($a/c \ll 1$) surface flaws, their results for center-cracked plates are accurately appropriate. Hence, for $a/t \leq 1/2$ and $a/c \leq 1$, their results will give fair approximations for the high stress level range, that is, $\frac{\sigma}{\sigma_o} > 2/3$.

Following the same Ramberg-Osgood analysis associated with equations (38) and (39) but for plane strain and again adopting Shih's[18] parameters for A533B steel at 90°C (that is, σ_o = 60 ksi, E = 30 × 10^3, ksi $\bar{\bar{\alpha}}$ = 1.115, and n = 9.7) resulted in

$$\{ \ \} = \left\{ 3.30 \left(\frac{\sigma}{\sigma_o}\right)^2 + 3.5 \left(\frac{\sigma}{\sigma_o}\right)^{10 \cdot 7} \right\} \tag{52}$$

Fairing the stress bracket, equation (52), together with the former equation (51) near $\sigma/\sigma_0 = 2/3$ provided the results in the "Plane Strain" column in Table 1. The "Plane Stress" column gives tabular results for through cracks for comparison. The Table 1 results were consistently similar even though the plane stress values eventually deviated from the plane strain values; therefore, the methodology was extended to surface flaws in uniform stress fields in vessel walls.

D. <u>Analysis of Surface Flaws into Nonuniform Stress Fields</u>

Though the pressure stresses in a nuclear vessel wall induce (almost) uniform unperturbed stress fields, secondary stresses such as residual stresses, thermal transient stresses, faulted conditions, and so forth, result in high stress gradients through the wall. The secondary stresses are of great concern only in combination with pressure stresses; they cause yielding locally (at the surface of the wall). The above methods are inadequate to handle this situation accurately, and other analytic methods are not available currently to develop a method of equal accuracy. However, an approximate and conservative method may be advanced (as suggested to this author by Riccardella (see Appendix H of NUREG-0744)).

As noted with equation (37), $J_{applied}$ is roughly proportional to applied strain. Consequently, if thermal stresses (or other secondary stresses) are solved for by elastic analysis, the stress values are much too high (yielding should have occurred), but the implied strains are nearly correct. Therefore, computing implied strains, averaging them over the crack area of a surface flaw, then transforming them back to equivalent stresses by the Ramberg-Osgood relation, equation (38), for insertion into the preceeding surface flaw analysis should give reasonable results. Tentatively, the results of such a superposition are judged to be a conservative method of handling secondary stresses. This matter should bear some further study.

Consistent with the proportionality of $J_{applied}$ to strain, Reuter (see Appendix I of NUREG-0744), in performing a sensitivity analysis of the effect of variation of the stress-strain curves on stress brackets, also noted this proportionality to strain.

Indeed, for high stress levels (σ/σ_0 equal to or greater than yield) he has shown that rewriting the stress bracket in terms of strain has some merit. That observation is consistent with the above suggestion of using superimposed strain to treat superimposed secondary stress circumstances.

Table 1 Stress Correction Factors for J for Plane Stress (Through Flaws) and Plane Strain (Surface Flaws) With Ramberg-Osgood Hardening*

$\frac{\sigma}{\sigma_o}$	{Plane Stress}[1]	{Plane Strain}[2]
0	0	0
0.2	0.134	0.127
0.4	0.546	0.516
0.5	0.898	0.819
0.6	1.38	1.20
0.7	2.05	1.65
0.8	3.39	2.35
0.9	7.35	3.78
1.0	16.1	6.80
1.05	24.6	9.54
1.10	37.9	13.6
1.15	58.3	19.9
1.20	89.1	29.3

where:

$$J = \frac{\sigma_o^2 a}{E} \cdot \{\ \} \cdot [\text{geometric correction}]$$

[1]for through-wall flaws

[2]for surface flaws

* { }, the stress correction factors, are to be used in the equation: $J = \frac{\sigma_o^2 a}{E} \cdot \{\ \} \cdot [\ ]$ where [ ] is the appropriate geometric correction factor. Source: Reference 17.
For use with the stress-strain law:

$$\frac{\varepsilon}{\varepsilon_o} = \frac{\sigma}{\sigma_o} + \bar{\bar{\alpha}} \left(\frac{\sigma}{\sigma_o}\right)^n;$$

for typical A-533B steel at 93°C using

σ_o = 60 ksi, $\bar{\bar{\alpha}}$ = 1.115, and n = 9.7 (see NUREG/CP-0010, Shih, 1979).

APPLICATION OF J VERSUS T DIAGRAM TO NUCLEAR PRESSURE VESSEL CONDITIONS AND MATERIALS

As explained in the text with Figure 4, crack instability occurs when the $J\text{-}T_{applied}$ curve intersects the $J\text{-}T_{material}$ curve. In discussion of both the surface flaw and through flaw from the LEFM range into the fully plastic range, it was noted by equations (24), (30), (31), (32), (44), and (50), that $J\text{-}T_{applied}$ is a nearly straight line through the origin of a J versus T diagram. Summarizing all those equations, the slope is

$$\frac{J_{applied}}{T_{applied}} = \frac{\sigma_o^{\ 2} a}{E} \, [0.5 \text{ to } 1] \tag{53}$$

On the other hand, the region of the J versus T diagram for which the material property data are absolutely assured to be accurate by "J-controlled growth" criteria, equation (18), is

$$\frac{J}{T}_{material} = \frac{\sigma_o^{\ 2} b}{E\omega} \tag{54}$$

where $\omega \geq 5 \gg 1$

In practice, the postulated flaw sizes, a, for analytical purposes are likely to be the order of 1/4 to 1 times the vessel wall thickness for surface and through flaw sensitivity studies, whereas the remaining uncracked ligaments, b, in experimental specimens are likely to be a much smaller fraction of the vessel wall thickness. Thus, to compare equations (53) and (54), it is noted that

$$a \, [0.5 \text{ to } 1] \gg \frac{b}{\omega} \tag{55}$$

or the loading line for $J_{applied}$ versus $T_{applied}$ will not intersect the materials curve in the region where $\omega \geq 5$. However, both the $J\text{-}T_{material}$ analysis, and $J\text{-}T_{applied}$ analyses are assured to be correct up to the experimental J levels where $\omega > 5$. Therefore, at that level there is still an assured safety margin factor on T; in fact by the inequality (55), that is

$$\text{margin} = \frac{T_{material}}{T_{applied}} \doteq \frac{a\,[0.5 \text{ to } 1]\omega}{b} >> 1 \tag{56}$$

which is a safety margin against instability considering equation (9).

Alternatively, it can be argued that since there is a fair margin against instability at the J level where $\omega \rightarrow 5$ in the test data, loading would necessarily have to increase further, raising the apparent J, in order for instability to occur. Using the linear (cummulative) extrapolation of the data as suggested in Figure 3, loading would at least progress beyond the point where the extrapolated data intersect the line representing equation (54) with b replaced by a. At that point, the analysis of the postulated vessel flaw is at a J-level where $\omega \geq 5$ so the analysis is still accurate. The load or pressure corresponding to J at that point can be found from the $J_{applied}$ equations. Some further unknown margin exists at still greater loads since instability has not yet ensued even though at high loads neither "J-controlled growth" nor the analysis method are absolutely assured.

A. Application to End of Life by Irradiation Damage to Actual Vessels

Considering the above discussion, the former (more conservative) criteria, rather than the alternative, will be adopted here as a limiting (safe) J-level for purposes of safety assessments. Both the available data on irradiation-damaged material and prospects of data from surveillance and other programs are limited to test specimen sizes of uncracked remaining ligaments, b, of slightly over 1 in. Inserting that size into equation (54) along with other typical irradiation-damaged material properties (for example: $\sigma_o \geq 85$ ksi; $E = 30 \times 10^3$ ksi) with $\omega = 5$ gives

$$\frac{J_{material}}{T_{material}} = \frac{\sigma_o^{\,2} b}{E\omega} = \frac{(85)^2(1)}{30(5)} \cong 50 \ \frac{\text{in.-lbs}}{\text{in.}^2} \tag{57}$$

Figure 13 is a J versus T diagram showing some of the available irradiated J-R curve data, as well as some low toughness unirradiated data. The curves shown were reported by Loss (see Part II, Appendix E of NUREG-0744) and the experimental details and the method of development can be found in the publication. It is sufficient to note here that in the opinion of this author they are properly obtained data meeting the conditions for "J-controlled growth" over the full range for which the curves are plotted. Indeed, Loss and his associates should be commended for this work.

A J/T = 50 line was plotted on Figure 13 intersecting the material data curves. Each curve has associated with it a number, which is the Charpy impact upper shelf energy (in ft-lbs) from tests of the same material and condition. Thus in the neighborhood of the Code-significant Charpy energy of 50 (that is, the range 35 to 78 ft-lb), it was noted that the J-levels at the intersections varied significantly. Also it was noted that these intersections with J/T = 50 were at J-levels considerably above the critical, J_{Ic}, values for these materials (sometimes as much as 3 times). Hence, J_{Ic} is far too conservative and not directly connected to crack instability for reasonable judgments of actual reactor safety for upper shelf conditions.

On the other hand, linear extrapolation of the material data curves from the J/T = 50 line to loading J/T curves for vessel postulations (usually J/T > 500), indicated a modest increase in J for instability above the J/T = 50 values. Consequently, the J/T = 50 intersection values, denoted J_{50} in further discussion here, appear to be reasonable (slightly conservative) for use in reactor vessel analysis. That is to say that these J_{50} values, established from J-R curve tests of actual vessel material including weld metal, are proposed and recommended as reasonable limiting values for vessel analysis for the current state of the art.

B. The Possibility of a Charpy Correlation With J_{50} Values

The previous discussion, recommending J_{50} values measured directly from a plane strain J-Integral R-curve test, did so for very relevant reasons. The test directly measures crack extension, Δa, behavior for increases in applied

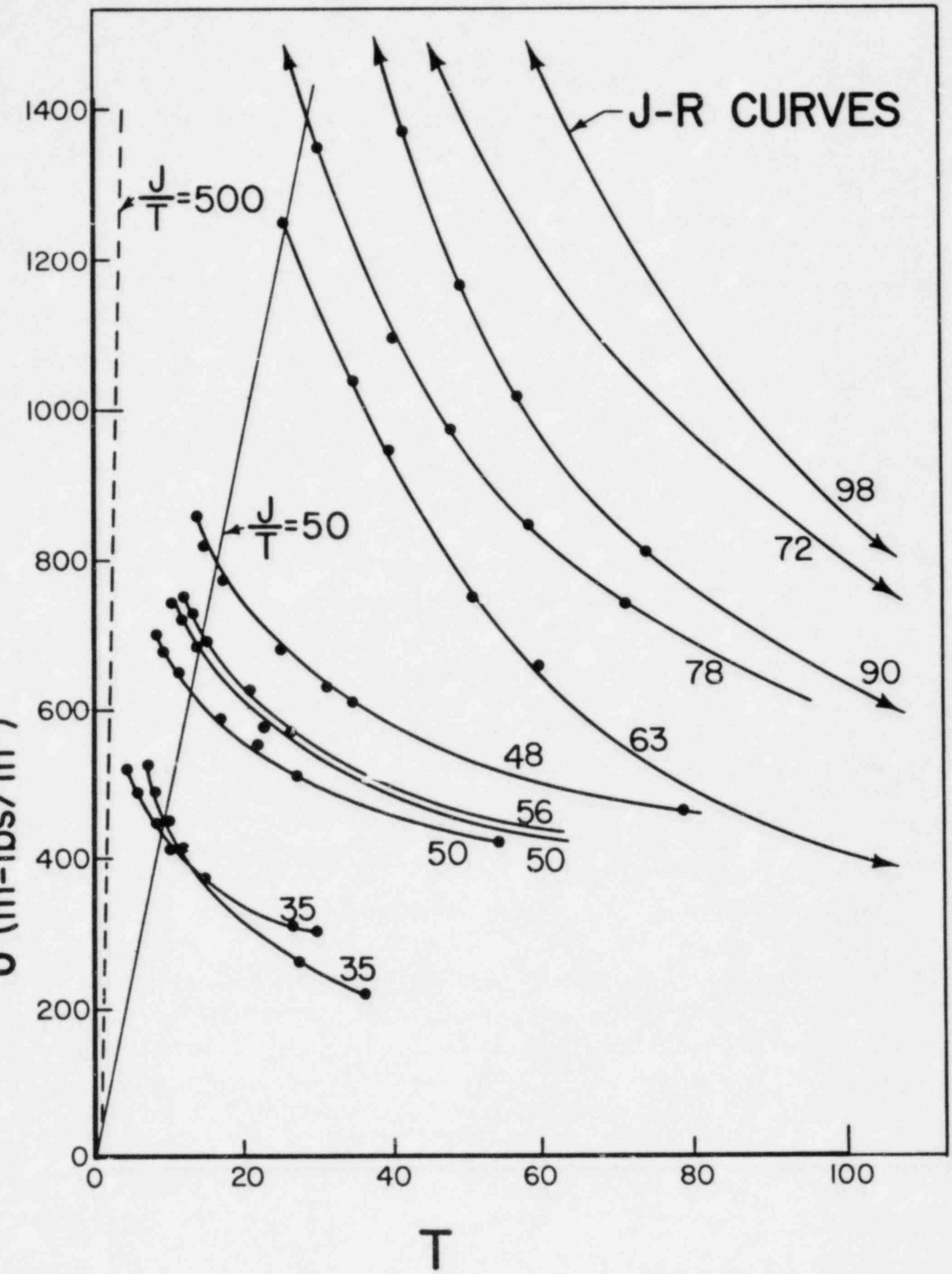

Figure 13 Typical data from Loss on irradiated nuclear vessel materials

J under rigorous J-controlled conditions. The results are directly applicable for estimating the instability of crack extension under local crack tip conditions of plane strain with J-controlled growth for postulated vessel cracks. Short of extensive testing of full scale vessels with cracks, it is the most direct and rigorous approach available.

Correlations or data extrapolations, though tempting, do not have an important role except in cases where no other avenue of approach exists, and even then only with a vast amount of statistical data available to illustrate exceptions. One potential case is that of existing reactors with doubtful material chemistries, including welds, whose surveillance capsules do not contain material samples from which proper J-R curves may be obtained. Some capsules have only Charpy bars and no other way to establish J-R curve properties.

For such an extreme case, the question is: can the Charpy test in some way be correlated to the relevant property for analytical judgments, J_{50}, no more, no less? As a consequence, Loss's data were plotted on Figure 14 to explore this possibility. For both irradiated and unirradiated base metal and weld metal, a scatter band of data resulted. The scatter band was fairly broad, but its lower boundary seemed well enough defined to provide hope that such a correlation may be possible for use where J-R curves are impossible to obtain.

It is noted that the data are from a single source and are not a very numerous (statistically significant) sample. So as hopeful as one may view this attempt at correlation, it remains to be firmly established.

Indeed if it is established as a correlation, it remains from statistical considerations to determine an adequate margin between the lower boundary of the data down to acceptable J_{50} values for use in analysis to assure safe utilization. This judgment is left for others after an adequate data base is available.

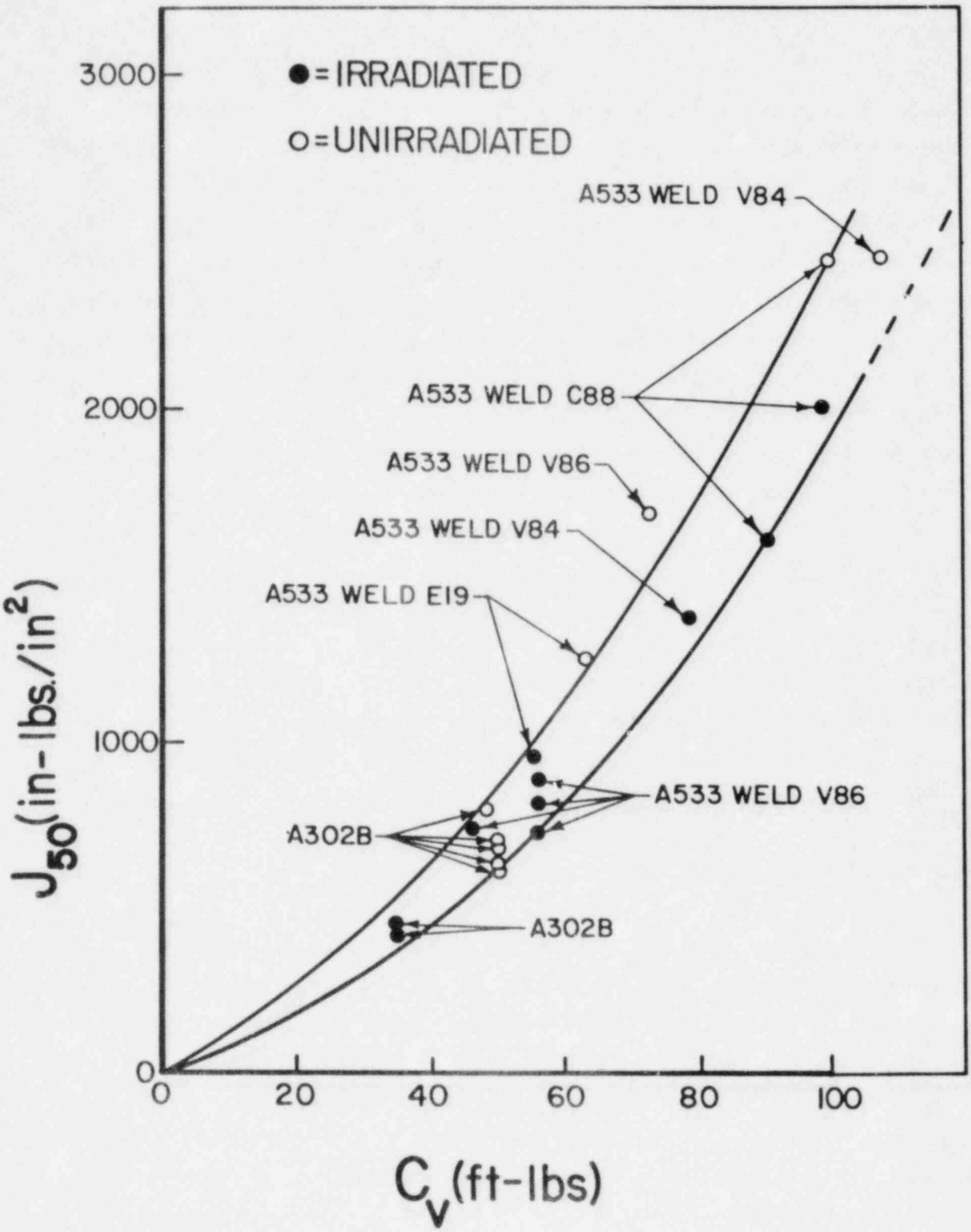

Figure 14 An attempted correlation of J_{50} values with Charpy upper shelf values from data by Loss

C. The Adequacy of the Current Data Base

The NRC has recognized for more than 5 years that J-R curves on irradiated-damaged material were going to be needed by NRC to make adequate safety judgments. NRC has also recognized the need for establishing adequate hot cell test procedures to assure J-R curves of suitable quality. However, progress in establishing an adequate data base has been slow. Although a J-R curve measurement procedure, the single specimen compliance method, was developed,[20] it has not been evaluated by round-robin testing. Standardization of this or any other J-R curve test method by the ASTM is even further away. It is significant that fracture toughness specimens in nuclear RPV surveillance programs involve several designs (for example, the IX WOL specimen, round CT, and so forth). Thus far, there have been no reports on the value of J-R curves derived from them, even though some existing older reactors are approaching the 50 ft-lb Charpy Code limit.

Consequently, it is noted that analysis presented herein, if judged relevant and timely, may suffer from an inadequate supporting data base. The J and T analysis formulas herein admittedly are at best good approximations, but at this time their precision is consistent with the best available methods for obtaining supporting data.

SUMMARY DISCUSSION AND CONCLUSION

The analysis methods developed in this report have attempted to combine several objectives. The methods suggested are first logical extensions of LEFM Code methods for flaw analyses in nuclear pressure vessels, making use of established elastic-plastic fracture mechanics methodology so as to be quantitatively applicable to conditions above the transition temperature. This has been done by making use of "tearing instability" concepts under "J-controlled growth" conditions to formulate crack instability criteria which are not overly conservative. The method is integrated with the use of J-R curves, which are the only available and widely accepted direct quantitative fracture properties characterization for above transition temperature conditions.

Staying within the J-controlled growth region for material properties tests, specifically for bend or compact specimen J-R curves, is shown to suggest limiting the loading on postulated vessel cracks to an applied J-level, J_{50}, where the test data intersect J/T = 50. This assures conservatively avoiding crack instability by the tearing mode. At near or below the transition temperature, the cleavage mode bears other considerations. On the other hand well above the transition temperature, which is usually consistent with nuclear vessel normal operating conditions, cleavage is avoided. As a further expedient for situations such as some surveillance programs where only Charpy specimens are available, it is shown that upper shelf Charpy energies seem to correlate with J_{50} values. This correlation and other data requirements suggest developing a broader data base.

The analysis and resulting equations developed here for applied values of J, T, and J/T are appropriate approximations permitting the separation of stress level effects and geometrical effects into independent factors. This has led to clearly delineating the regions of interest on J versus T diagrams for the location of potential crack instability points for postulated vessel cracks at or above about J/T = 500. Thereafter, once a safe value is selected, such as limiting J to a value J_{50} (for J/T $\leq$ 50), the approximations have served their purpose. Nevertheless, they are clearly and conservatively developed herein and are suggested as sufficiently accurate for broad usage with the advantages of simplicity and familiarity to fracture mechanics practitioners.

REFERENCES

Documents marked with an asterisk may be ordered from the NRC/GPO Sales Program, Washington, DC 20555 and/or the National Technical Information Service, Springfield, VA 22161.

1. ASME Section XI Task Group on Flaw Evaluation, "Flaw Evaluation Procedures," T. V. Marston, ed., EPRI Special Report NP-719-SR, August 1978.

2. P. C. Paris, H. Tada, A. Zahoor, and H. Ernst, "A Treatment of the Subject of Tearing Instability," USNRC Report NUREG-0311, August 1977. (See also ASTM, STP 668, 1979.)*

3. J. W. Hutchinson and P. C. Paris, "Stability Analysis of J-Controlled Crack Growth," ASTM, STP 668, 1979.

4. "Proceedings of the CSNI Meeting on Plastic Tearing Instability," compiled by P. C. Paris , USNRC Report NUREG/CR-0100, January 1980.*

5. C. F. Shih, "An Engineering Approach for Examining Growth and Stability in Flawed Structures," a contribution in Reference 4.*

6. J. W. Hutchinson, "Singular Behavior at End of Tensile Cracks in Hardening Material," Journ. of Mech. and Phys. of Sol., Vol. 16, No. 1, January 1968.

7. J. R. Rice and G. F. Rosengren, "Plane Strain Deformation Near a Crack Tip in Power-Law Hardening Material," Journ. of Mech. and Phys. of Sol., Vol. 16, No. 1, January 1968.

8. J. R. Rice, "A Path Independent Integral and the Approximate Analysis of Strain Concentration by Notches and Cracks," Trans. of ASME, Journ. of Appl. Mech., Vol. 35, pp 379-386, 1968.

9. P. C. Paris, "Fracture Mechanics in the Elastic-Plastic Regime," ASTM, STP 631, 1977.

10. E. S. Folias, Int. Journ. of Fract. Mech., Vol. 1, p. 104, 1965.

11. F. Erdogan and J. J. Kibler, Int. Journ. of Fract. Mech., Vol. 5, p. 229, 1969.

12. S. Krenk, "Influence of Transverse Shear on Plasticity Around an Axial Crack in a Cylindrical Shell," Trans., 4th Int. Conf. on Structural Mech. in Reactor Technology, San Francisco, paper G5/3, 1977.

13. D. P. Rooke and D. J. Cartwright, "Compendium of Stress Intensity Factors," Her Majesty's Stationary Office, London, 1976.

14. H. Tada, P. Paris, and G. R. Irwin, The Stress Analysis of Cracks Handbook, Del Research Corporation, 226 Woodbourne Drive, St. Louis, MO 63105, 1973.

15. J. W. Hutchinson, A. Needleman, and C. F. Shih, "Fully Plastic Crack Problems in Bending and Tension," Fracture Mechanics, N. Perrone and others, ed., Univ of Virginia Press, 1978.

16. A. Zahoor, contribution in "Further Results on the Subject of Tearing Instability -II" (prepared by H. Tada and P. C. Paris), USNRC Report NUREG/CR-1220, January 1980.*

17. H. Tada and P. C. Paris, "Tearing Instability Analysis Handbook," USNRC Report NUREG/CR-1221, January 1980.*

18. C. F. Shih and J. W. Hutchinson, "Fully Plastic Solutions and Large Scale Yielding Estimates for Plane Stress Crack Problems," Trans. of ASME, Journ. of Engin. Mat. and Tech., Vol. 98, No. 4, October 1976.

19. R. S. Burrington, Handbook of Mathematical Tables and Formulas, Handbook Publishers Inc., Sandusky, Ohio, 3rd ed., 1949.

20. G. A. Clarke, W. R. Andrews, D. W. Schmidt, and P. C. Paris, "Single Specimen Tests for J_{Ic} Determination", ASTM, STP 590, 1976.

NOTATION

a	Crack length (for through thickness cracks) or crack depth (for surface cracks).
a_{eff}	Effective crack size with a plastic zone correction added.
a_o	Initial crack size prior to growth.
Δa	Crack length change.
b	Uncracked ligament size.
c	Half the surface length of a surface crack.
E	Modulus of elasticity.
$f(a/c)$	A surface flaw geometry correction.
f^*	A coefficient in a hardening stress bracket.
$F(a/c)$	A coefficient in a hardening stress bracket.
$g(a/t)$	A coefficient in a hardening stress bracket.
$G(a/t)$	A coefficient in a hardening stress bracket.
G^*	A coefficient in a hardening stress bracket.
J	Rice's J-Integral, total energy (elastic and plastic)
J_{Ic}	Critical value of J-integral
J_{appl}	The intensity of the crack tip emergency field, J, applied.
J_{mat}	The material resistance value of J for an observed crack length change, Δa.
K	The intensity of an elastic crack tip stress field.
K_{Ic}	Fracture toughness
K_{Id}	Dynamic fracture toughness
n	A hardening exponent (for describing material properties).
P	Applied load.
r	Radial distance from a crack tip.
r_y	A plastic zone size.

R	Radius of a pressure vessel.
s	Arc length on a contour around a crack tip.
t	Wall thickness of a pressure vessel.
T	Tearing modulus.
T_{app}	Applied tearing modulus.
T_{mat}	The material resistance to tearing modulus or an observed crack length change, Δa.
T_i	Applied traction (stress).
u_i	Displacement corresponding to an applied traction.
U	Elastic system energy stored (corresponding to deformation plasticity theory).
W	Plate or test specimen width.
x,y	Rectangular coordinates, coplanar with and perpendicular to a crack surface, respectively.
Y	A geometrical correction for crack tip field intensity in shells (a function of $\lambda = a/\sqrt{Rt}$).
$\bar{\alpha}$, $\bar{\bar{\alpha}}$	Coefficients in hardening laws for stress-strain curves.
β	Stress state (plane stress versus plane strain) coefficient in a plastic zone correction.
σ	A coefficient adjusted for stress state in relating δ to J.
Γ	A contour around a crack tip.
δ	Crack opening stretch (displacement).
δ_p	Load point displacement.
Δ	Applied displacement (loading).
ε	Strain.
ε_o	Flow Strain.
ε_{ij}	Components of strain.
E_{ij}, $\bar{\Sigma}_{ij}$	Functions of θ and n in power hardening fields of strain and stress.

θ	Angular coordinate measured from the linear extension of a crack plane.
λ	A shell parameter, $a/\sqrt{Rt}$.
σ	Applied (tension) stress.
σ_{ij}	Components of stress.
σ_o	Flow stress (in tension).
σ_{eff}	Net ligament nominal (effective) stress.
ϕ_o	The complete elliptic integral of the second kind (see Reference 19).
ψ^*	A coefficient in a hardening stress bracket.
ω	Hutchinson's J-controlled growth validity assurance parameter.
$'$	As, for example, Y'; derivative with respect to the argument.
{ }	Stress brackets or factors in equations for J and T applied.
[ ]	Geometry brackets or factors in equations for J and T applied.

NRC FORM 335 (7-77) U.S. NUCLEAR REGULATORY COMMISSION **BIBLIOGRAPHIC DATA SHEET**	1. REPORT NUMBER (Assigned by DDC) NUREG-0744, Vol. I & II Rev. 1
4. TITLE AND SUBTITLE (Add Volume No., if appropriate) Resolution of the Task A-11 Reactor Vessel Materials Toughness Safety Issue, Volumes I and II	2. (Leave blank)
	3. RECIPIENT'S ACCESSION NO.
7. AUTHOR(S) Richard Johnson	5. DATE REPORT COMPLETED MONTH October YEAR 1982
9. PERFORMING ORGANIZATION NAME AND MAILING ADDRESS (Include Zip Code) United States Nuclear Regulatory Commission Office of Nuclear Reactor Regulation Division of Safety Technology Washington, D. C. 20555	DATE REPORT ISSUED MONTH October YEAR 1982
	6. (Leave blank)
	8. (Leave blank)
12. SPONSORING ORGANIZATION NAME AND MAILING ADDRESS (Include Zip Code) United States Nuclear Regulatory Commission Office of Nuclear Reactor Regulation Division of Safety Technology Washington, D. C. 20555	10. PROJECT/TASK/WORK UNIT NO.
	11. CONTRACT NO.
13. TYPE OF REPORT Technical Report	PERIOD COVERED (Inclusive dates)
15. SUPPLEMENTARY NOTES	14. (Leave blank)

16. ABSTRACT (200 words or less) This report provides the NRC position with respect to the reactor pressure vessel safety analysis required according to the rules given in the Code of Federal Regulations, Title 10. An analysis is required whenever neutron irradiation reduces the Charpy V-notch upper shelf energy level in the vessel steel to 50 ft-lb or less. Task A-11 was needed because the available engineering methodology for such an analysis utilized linear elastic fracture mechanics principles, which could not fully account fortheplastic deformation or stable crack extension expected at upper shelf temperatures. The Task A-11 goal was to develop an elastic-plastic fracture mechanics methodology, applicable to the beltline region of a pressurized water reactor vessel, which could be used in the required safety analysis. The goal was achieved with the help of a team of recognized experts. Part I of this volume contains the "For Comment" NUREG-0744 originally published in September 1981 and edited to accommodate comments from the public and the NRC staff. Part II of this volume contains the staff's responses to, and resolution of, the public comments received. This report completed the staff resolution of the Unresolved Safety Issue A-11, "Reactor Vessel Materials Toughness."

17. KEY WORDS AND DOCUMENT ANALYSIS

Unresolved Safety Issue A-11
Reactor Vessel Materials Toughness

17a. DESCRIPTORS

17b. IDENTIFIERS/OPEN-ENDED TERMS

18. AVAILABILITY STATEMENT	19. SECURITY CLASS (This report)	21. NO. OF PAGES
Unlimited	Unclassified	
	20. SECURITY CLASS (This page) Unclassified	22. PRICE $

NRC FORM 335 (7-77)

☆U.S. GOVERNMENT PRINTING OFFICE: 1982-381-297:2474

UNITED STATES
NUCLEAR REGULATORY COMMISSION
WASHINGTON, D.C. 20555

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