ENCLOSURE 2

# TECHNICAL EVALUATION REPORT (TER) REVIEW OF BAW-2148P, REV. 1, "LOW UPPER-SHELF TOUGHNESS FRACTURE ANALYSIS OF REACTOR VESSELS OF ZION UNITS 1 AND 2 FOR LOAD LEVEL A & B CONDITIONS" DOCKET NOS. 50-295 AND 50-304

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## ABSTRACT

This report contains a review of BAW-2148P, Rev. 1, describing low-upper-shelf safety margin analyses of the Zion Units 1 and 2 reactor pressure vessels. The major aspects of this review concern the accuracy of the safety margin calculations, the choice of a reference tem- rature for the calculations, the meaning of the phrase "equivalent margins," the variability pper contents of WF-70 weld metals, the possible presence of Atypical weld metal in the Z. sels, the omission of a plastic zone size correction to the thermal component of $K_I$ in the utility's analyses, temperature effects on J-R curves and elastic moduli, consistency in the values of elastic moduli used in the different parts of the calculations, and revisions to the Zion fluence estimates not considered in the utility's submittal. Based on a net J-R specimen thickness of 0.8 in. and providing that the reference temperature of 530°F is actually the vessel wall temperature at normal operat , the Zion vessels satisfy both Criteria #1 and #2 of Code Case N-512 for Level A and B gs, albeit with little or no excess margins. However, if the reference temperature we r, that would not be the case.

## INTRODUCTION

### Background

The purpose of this Technical Evaluation Report (TER)is to provide an engineering review, including regulatory conclusions and recommendations, concerning the analysis submitted to the Nuclear Regulatory Commission (NRC) by Commonwealth Edison Co.[1] of the safety margins against ductile fracture for the reactor pressure vessels of the Zion Units 1 and 2 nuclear power plants. This analysis is made necessary by the requirements[2] of 10CFR50 which state that a reactor pressure vessel containing materials that are expected to have charpy upper shelf impact energy values that become less than 50 ft. lbs. due to irradiation damage must be evaluated analytically to determine if safety margins against ductile fracture are still adequate.

The necessity to develop analysis methods and criteria for insuring adequate margins against ductile fracture prompted an NRC study on the subject of appropriate analysis methods[3] which was completed in 1982. Subsequently, the NRC requested[4] that the American Society of Mechanical Engineers (ASME) develop code criteria for, "setting safety margins to avoid reactor pressure vessel failure under elastic-plastic fracture conditions." The ASME accepted this task and after due deliberation prepared a draft report[5] as well as transmitted recommended criteria to the NRC.[6] An appendix to Section XI of the ASME Boiler and Pressure Vessel Code is in process and a Code Case on the subject of low upper-shelf safety margins has been issued.[7] The analysis methods and criteria applied in Ref. 1 are intended to be consistent with those described in Ref. 7.

A technical and regulatory overview of the low upper-shelf toughness safety margin issue,[8] prepared under NRC sponsorship, was published in 1990. The NRC's regulatory requirements pertaining to the low upper shelf toughness safety margin issue are contained in 10CRF50, Appendix G.[2] In this appendix, paragraph IV.A.1 states that, "Reactor vessel beltline materials ... must maintain upper-shelf energy throughout the life of the vessel of no less than 50 ft-lb (68J), unless it is demonstrated in a manner approved by the Director, Office of Nuclear Reactor Regulation, that lower values of upper-shelf energy will provide margins of safety against fracture equivalent to those required by Appendix G of the ASME Code." Although unstated, it is understood that the demonstration required by paragraph IV.A.1 can be analytical, based on existing material property data and ASME code criteria. Paragraph V.B states that, "Reactor vessels may continue to be operated only for that service period within which the requirements of Section IV of this appendix are satisfied." Paragraph V.C then states that, "In the event that the requirements of Section V.B of this appendix cannot be satisfied, reactor vessels may continue to be operated provided all of the following requirements are satisfied:

(1) volumetric examination...,

(2) additional evidence of the fracture toughness of the beltline materials...,

(3) analysis ...

The submission contained in the report being reviewed[1] responds to paragraph IV.A.1 in that it contains analyses intended to demonstrate adequate safety margins for the case of CVN approaching 50 ft-lbs. The phrase, "margins of safety against fracture equivalent to those required by Appendix G of the ASME Code" apparently originated in and exists only in, Ref. 2. It was not used in Ref. 4 which defined NRC's request of ASME to develop low upper shelf safety margin criteria, nor did it appear in the response from ASME to the NRC, Ref. 6.

Consequently, it must be understood that no mathematical demonstration of equivalent margins, in terms of identical failure probabilities, exists between Appendix G of Section III and Code Case N-512 of Section XI of the ASME Code. The equivalence of safety margins is basically qualitative in the sense that in the best judgement of ASME Code personnel, the specified safety margins and criteria in Appendix G and Code Case N-512 are both equally appropriate and adequate. A discussion of the important factors considered in selecting the safety margins in Appendix G and Code Case N-512 appears in Ref. 5.

In addition to 10CFR50, Appendix G, the NRC issued Regulatory Guide 1.99, Revision 2, "Radiation Embrittlement of Reactor Vessel Materials,"[9] which describes procedures intended to be conservative, for estimating both the increase in reference transition temperature, $RT_{NDT}$, and the decrease in charpy upper shelf impact energy, USE as functions of product form, neutron fluence and material chemistry. NRC Generic Letter 88-11[10] made the use of R. G. 1.99, Ref. 2, mandatory for estimating upper shelf energy decreases unless the licensee can justify the use of other methods. No consideration was given to the possibility that other methods might be more conservative.

An important aspect of estimating irradiation damage to reactor pressure vessel steel is selecting the copper content to use in the damage estimate. Because the Linde 80 flux used in fabricating the low upper shelf welds of concern is supposedly "neutral," in the sense that it does not affect the weld chemistry,[11, 12] the NRC has assumed that weld copper content is determined solely by weld wire heat number.[13] This assumption is stated explicitly in the latest version of 10CFR50.61.[14] The assumption becomes relevant to the selection of chemistry contents for the governing materials in the Zion Units 1 and 2 reactor pressure vessels and will be discussed later in that context.

The development of low-upper-shelf code criteria in the ASME Section XI Working Group on Flaw Evaluation (WGFE) was a joint effort between representatives of industry and the Nuclear Regulatory Commission. It was agreed that the code criteria would specify a conservative estimate of toughness for Level A, B, and C loading conditions, and that the NRC would draft a regulatory guide describing in more detail an acceptable method for making such an estimate.[15] The draft regulatory guide has been prepared[16] and will soon be issued for public comment.

In July 1990 the Yankee Atomic Electric Company submitted a pressure vessel evaluation report[17] to the NRC as part of a license renewal submittal for the Yankee Rowe nuclear power station. A consequence of this submittal was the finding that the reactor pressure vessel currently contained material with a Charpy upper shelf impact energy that could be as low as 35.5 ft. lbs., but that the evaluation required by 10CFR50, Appendix G had not been performed.[13] This finding led to a concern on the part of the NRC staff that there might be other plants out of compliance with the provisions of 10CFR50.60, 10CFR50.61, and Generic Letter 88-11 (Ref. 10). To determine the current status of reactor pressure vessel integrity data and evaluations, the NRC issued Generic Letter 92-01 (Ref. 13). This letter required the nuclear utilities to furnish up-to-date vessel integrity related data, including weld chemistry, weld wire heat number and surveillance data. Ref. 13 stated that if surveillance data imply a greater ΔUSE than estimated by R. G. 1.99, Rev. 2, this fact, and how it has been considered, must be reported. In reiterating the provisions of 10CFR50, Appendix G, the phase, "equivalent margins of safety" was used, but no explicit definition was given. Replies were required by July 7, 1992.

Licensee replies to Generic Letter 92-01 (Ref. 13) indicated that, based on plant specific and integrated surveillance data, all vessels currently satisfy the 50 ft. lb. minimum USE criterion. However, based on R. G. 1.99, Rev. 2 (Ref. 9), 15 plants currently have USE values less than 50 ft. lbs.[18] In response to a NRC Commission request on this subject of July 19, 1991, the NRC staff furnished the lists of plants given in Tables 1 and 2 that, according to Ref. 9 currently do not, or before end-of-life (EOL) will not, meet the 50 ft. lb. criterion. The NRC staff found that additional information would be required to determine if plant specific analyses used acceptable methods to estimate irradiated USE values.[18] This additional information includes the experimental basis for estimating the average ratio of transverse to longitudinal USE values for plate, the basis for initial $RT_{NDT}$ estimates (especially for many BWR plants that lack unirradiated USE dat: ) and explanations for apparent inconsistencies between currently reported and previously repor ed data.[18] At the time Ref. 18 was issued, some utilities had already commenced analyses to determine if their vessels satisfied the criteria given in ASME Section XI Code Case N-512 (Ref. 7). These plants are listed in Table 3. It is the utility submittals for the plants listed in Table 3 'hat are being reviewed for NRR by ORNL.

On September 2–3, 1992, a meeting between NRC and industry representatives was held to discuss pressure vessel integrity issues. The NRC staff suggested that the industry perform generic bounding analyses to investigate low upper-shelf (LUS) safety margins. Subsequently, the NRC commissioned the Heavy Section Steel Technology Program at ORNL to perform such analyses. These analyses have since been completed and a report issued.[19] Following the September 1992 meeting, the Nuclear Management and Resources Council (NUMARC) began coordinating the industry responses to Generic Letter 92–01. The low-upper-shelf analyses were scheduled to be submitted to the NRC between January and April 1993. The NRC staff plans to complete its reviews of all the Generic Letter 92–01 submittals by the end of 1993.[18]

### Approach to Technical Review

The approach taken to this technical review consisted of several steps, the first of which was a preliminary reading of Ref. 1, during which a listing was made of missing information, technical and safety related questions and analysis input and results requiring some degree of verification. A Request for Additional Information (RAI) was then prepared and forwarded to the NRR technical monitor (TM). Following discussion with the TM, a modified RAI was transmitted to the utility by NRR. After receipt of the utility's response, additional more detailed evaluations and some checking and sensitivity calculations were performed, leading to the conclusions and recommendations stated later in this report.

The main issues identified during the preparation of the RAI for Zion Units 1 and 2 were the following:

1. Meaning of the phrase, "equivalent margins;"
2. Implicit reliance on operability of relief values;
3. There is a material temperature below which cleavage can occur;
4. Comparison of the Eason-NRC and Eason-B&W correlations;
5. Atypical weld metal
   a) Consideration;
   b) Initial upper shelf energy (USE);
   c) Applicability of the Eason Correlations;

6. WF-70 weld metal

a) Mean copper content;
b) Inclusion of margin in estimate of copper content;
c) The controlling weld is stated to be a circumferential WF-70 weld, but the calculations are performed for an axially oriented flaw.

## RESOLUTION OF ISSUES

### Equivalent Margins

The present meaning of the phrase "Equivalent Margins" has been discussed in the introduction to this TER. Pragmatically, safety margins for preventing failure by ductile tearing instability are apparently considered by the NRC to be equivalent to those for preventing brittle fracture required by Appendix G of Section III of the ASME Code if they also satisfy criteria established by the ASME Code.

### Relief Valves and Safety Margins

In selecting the combinations of reference pressures and degrees of conservatism with respect to the mean value of tearing resistance to be specified in Code Case N-512, consideration was given to the fact that the pressure relief valves and head seal greatly reduce the probability of pressures exceeding certain limits.[20] Past practice has been to consider pressure relief valves as a means of reducing the probability of overloads to the vessel, but not as a substitute for the strength that should be inherent in the vessel itself. Nevertheless, it does seem proper to consider the existence of pressure relief valves and the head seal when choosing factors of safety. Preliminary calculations performed by the ASME Section XI WGFE showed that the required upper shelf energy is sensitive to several factors, which must therefore be carefully considered. These factors include vessel wall thickness, pressure in the crack, thermal stress, plastic zone size effects, the assumption of plane strain vs. plane stress (plane strain is more accurate), flaw orientation, the reference pressure for the safety factor calculation, and the statistical significance of the toughness values (mean or lower bound). Since calculated instability pressures would be above the safety valve settings and therefore of low probability, it seems reasonable to consider reducing the required safety factors on pressure as the probability of exceeding the selected toughness value increases.[20] It was recognized that criteria are needed both to limit the amount of ductile crack extension and to prevent tearing instability. It was also recognized that J-R curves exhibit scatter and size effects only partially understood, making extrapolations for instability calculations subject to error. Therefore, it was decided by the WGFE to formulate criteria in terms of conservative measures of toughness for Levels A, B, and C, and to replace the instability calculations necessary to determine full safety margins with calculations demonstrating flaw stability for specified load margins. The latter calculations require less J-R curve extrapolation. Compensating adjustments were made to the specified load margins, based on the expected ratio of lower bound toughness to mean toughness, so that results in terms of safety would remain roughly the same as those obtained when calculating instability loads based on mean toughness. In developing the criteria for Levels C and D, it was deemed desirable to specify different safety criteria for the two load categories, because of the differences in the associated event probabilities and structural performance requirements.

For Levels A and B, the reference flaw is the Appendix G flaw, oriented along the weld of concern or having whatever orientation in low upper shelf base metal is most conservative. A conservative measure of toughness is also employed. A reference pressure called the accumulated pressure[21] (also known as the accumulation pressure) $P_{acc}$, is used for safety verification. The accumulation pressure is the highest pressure that can occur in the system, as estimated by a calculation that includes the effects of pressure relief valve settings and fluid discharge rates through those valves. The accumulation pressure is limited to 10% above component design pressure, so that for a vessel design pressure of 17 MPa (2500 psi) the accumulation pressure cannot exceed 19 MPa (2750 psi). The limited crack growth criterion requires that at a pressure of 1.15 $P_{acc}$ and specified thermal loading, stable crack growth must not exceed 2.5 mm (0.10 in.). The stability criterion requires that at a pressure of 1.25 $P_{acc}$ and the same thermal load, ductile flaw growth must remain stable.

For Levels C and D, the reference flaw depth range is from zero to one-tenth of the base metal wall thickness, plus the clad thickness, but not to exceed 25.4 mm (1.0 in.). Flaw shapes and orientations are the same as for Levels A and B. The reference toughness for Level C is conservative, while for Level D it is the mean toughness. Loads are as determined by plant specific analyses for the specified load categories, with no additional safety factors. For Level C, stable crack growth must not exceed 2.5 mm (0.10 in.) and the flaw must remain stable. For Level D, the flaw must either remain completely stable or it must not extend beyond a/t of 0.75 and the remaining ligament must be safe against tensile instability.

### Lower Temperature Limit of the Upper Shelf

The procedure for calculating stress intensity factors due to steady state thermal loading originally published in WRC-175[22] and described in Section XI, Appendix G of the ASME Code[23] recognized that, in homogeneous bodies, thermal stresses, and therefore $K_{It}$ values, depend only on the temperature differences through the vessel wall but not upon the absolute temperatures. The analytical criteria for low upper shelf toughness safety margins given in Code Case N–512 (Ref. 7) made use of this fact and the fact that increasing temperature lowers the J–R curve. In Ref. 7, Level A and B loadings were described in terms of pressure and steady state cooling rate, with the intent that the vessel wall would be assumed to remain at the operating temperature even though stresses due to thermal loading were being considered. However, the advent of numerical combined thermal, stress, and fracture analyses has led to the calculation of decreasing crack tip temperatures that cause the J–R curve to rise but which may also exit the fully ductile upper shelf temperature range and enter the transition temperature range within which mode conversion to cleavage is possible. In performing such calculations, it should be determined whether or not the assumed duration of steady state cooling is real. If it is not, then considering the effect of temperature on the J–R curve is not justified. As a conservatism, Ref. 16 does not allow the consideration of crack tip temperature effects on the J–R curve for steady state thermal $K_I$ calculations. Ref. 1 has followed this guideline. If the steady state cooldown duration is real, then theoretically the elevation of the J–R curve caused by the decrease in crack tip temperature can be considered, as suggested in Ref. 19. However, the possibility of mode conversion to cleavage must also be considered. Ref. 7 does not address this issue. Ref. 16 suggests that the lower limit of the upper shelf temperature range, $T_{LUS}$, can be estimated as $RT_{NDT}$ + 50°F. The Zion submittal, Ref. 1 (see p. 4–5) more conservatively assumes that $T_{LUS} = RT_{NDT} + 120°F$. Even this assumption does not consider the possible influence of locally elevated crack tip strain rates caused by the ductile failure of grain sized ligaments near the crack tip. Estimates of $RT_{NDT}$ at T/4 for the Zion vessels at end-of-life (EOL)[24] are as high as 270°F, the value considered in Ref. 1. At the assumed rate of cooldown, 100°F/hr., it would take more than four hours to cool the vessel down to room temperature. Calculations with the FAVOR code [25] indicate that after three hours of cooling the temperature at T/4 would be about 280°F, well below $T_{LUS}$. Thus, generically there is a need to select an appropriate value of $T_{LUS}$ relative to $RT_{NDT}$, and to recognize that Level A and B transients passing below $T_{LUS}$ must also satisfy the criteria of Appendix G of Section XI.[23] This applies to the Zion vessels.

**Atypical Weld Metal**

One of the most important steps in performing a low upper shelf safety margin analysis is the identification of the governing material in the vessel. The governing material will depend upon product form (base metal or weld), orientation, copper content and fluence. The materials present in the Zion Units 1 and 2 reactor pressure vessels, and their data relevant to USE, are listed in Tables 2–1 and 2–2 of Ref. 1. The two materials that have the highest combinations of copper content and fluence, for both the longitudinal and circumferential orientations, are the weld metals designated Atypical weld metal and WF-70. The fluences for the two materials are identical, and Atypical weld metal has a slightly higher copper content, 0.41% compared to 0.35% for WF-70. Nevertheless, Atypical weld metal is not listed in Table 5–2 on page 5–7 of Ref. 1 and it is not designated as the controlling material. The USE values at T/4 estimated for Atypical weld metal and WF-70 by R. G. 1.99, Rev. 2 (Ref. 9) and listed in Tables 2–1 and 2–2 of Ref. 1 are higher for Atypical weld metal than for WF-70. This is the reason given on page 2–1 of Ref. 1 for not designating Atypical weld metal as the controlling material. However, no comparable values of EOL-USE for Atypical weld metal, estimated by the alternate method based on the B&W Owners' Group (B&WOG) integrated surveillance program data as described in Ref. 24, are given in Tables 2–1 and 2–2 of Ref. 1, even though such values are given for WF-70 and other materials. Consulting Tables B-31 and B-32 of Ref. 24, the unirradiated USE values for Atypical weld metal and WF-70 are given as 79 and 70 ft-lbs., respectively. Evidently the higher unirradiated USE of Atypical weld metal more than offsets the effect of its higher copper content, leading to a higher value of EOL-USE than for WF-70, as calculated by R. G. 1.99, Rev. 2. However, the B&WOG method for estimating irradiated USE values for Linde 80 welds described in Ref. 24 is based only on copper content and fluence and is independent of the initial USE. Consequently, the EOL-USE values for Atypical weld metal estimated in Ref. 24 are lower than those for WF-70 and less than 50 ft-lbs. Therefore, the nature of Atypical weld metal and whether or not it should be considered as the governing material in the Zion vessels has been examined further.

Table 4–1 of Ref. 24 contains a list of weld metals, data from which were used to develop the B&WOG correlation for estimating irradiated USE as well as a correlation for estimating $\Delta RT_{NDT}$. Included in the list is Atypical weld metal, identified as WF-209-1C. Tables 2–1 and 2–2 of Ref. 1 contain footnotes concerning Atypical weld metal, referring to a reference identified as BAW-10144-A.[26] Ref. 26 discusses the history of Atypical weld metal, from which a judgement about its significance can be developed.

Atypical weld metal had its known origin in the fabrication of two weldments as material sources for the Crystal River Unit 3 surveillance program.[26] The first weldment was fabricated in June 1971, and the second in March 1973. In 1978, it was discovered that the second weldment had atypical chemistry, specifically low nickel and high silicon, as indicated in Table 4. Values of copper were fairly high and variable, with the means of two different test series being 0.41 and 0.43%. A records search indicated that both weldments were supposed to have been made with Mn Mo Ni weld wire heat no. 72105[26] and the same lot of Linde 80 flux (presumably either lot no. 8773 or lot no. 8669, the same lots used for welds WF-209-1 and WF-70[11]). However, an incorrect coil of copper coated weld wire had evidently been mixed with the coils of heat no. 72105 and was used in fabricating the second CR-3 surveillance weldment.[26] The actual type of incorrect weld wire was probably AWS A5.18, E70S-1B.

Presumably based on chemistry measurements, the incorrect weld wire is only known to have been on the shop floor during the fabrication of the second CR-3 surveillance weldment.[26] However, presumably based on the use of weld wire heat no. 72105 for vessel fabrication, Atypical weld metal could have been used by B&W in fabricating the 12 reactor pressure vessels listed in Table 5. Although B&W acknowledges that there is no way to conclusively determine the extent or distribution of off chemistry weldments fabricated into vessels during the relevant time period, it also appears that the probability of Atypical weld metal actually having been fabricated into any of the 12 reactor pressure vessels is very low.[26] Nevertheless, the NRC required that the possible presence of Atypical weld metal be considered in calculating pressure-temperature (PT) limits, and the technical specifications for some of the plants were modified accordingly.

Ref. 1 states the position that Atypical weld metal is not controlling in the Zion Vessels, based on EOL-USE estimates made by the R. G. 1.99, Rev. 2, procedure and implicitly (by omission) ignoring lower estimates based on Ref. 24. However, it was the B&WOG's earlier recommendation that the latter estimates are more reliable.[27] Furthermore, as already noted, the NRC, by means of Ref. 13, requires that if surveillance data imply a greater $\Delta$USE than estimated by R. G. 1.99, Rev. 2, this fact and how it has been considered, must be reported. Ref. 1 is not in compliance with Ref. 13. Nevertheless, the analyses in Ref. 1 appear to have taken this situation into account indirectly. As stated on page 5–4 of Ref. 1, the calculation of $K_{Ip}$ was done with Eq. (3–2) which, as stated on page 3–3, applies to an axially oriented flaw. Nevertheless, the fluence at T/4 used for determining the reference J-R curve, as given on page 5–2 of Ref. 1, is $9.34 \times 10^{18}$ n/cm$^2$ which, according to Table 2–1 is the peak circumferential fluence for WF-70 in the Zion 1 vessel. The peak axial fluence for WF-70 in the Zion 2 vessel is lower by nearly a factor of three, $3.26 \times 10^{18}$ n/cm$^2$. Thus the fluence for the reference axial flaw in the longitudinal WF-70 weld in the Zion 2 vessel has been deliberately overestimated. As shown in Fig. 1, this overestimate of fluence has the effect of producing a reference J-R curve for the longitudinal WF-70 weld in the Zion 2 vessel, using Cu = 0.35% and f = 9.34, almost coinciding with the calculated J-R curve for the possible Atypical longitudinal weld in the same vessel, considering its higher copper content of 0.41%, but the actual lower peak fluence of f = 3.26. Consequently, the possible presence of Atypical weld metal in the Zion Units 1 and 2 vessels has been taken into account to a close approximation while preserving the stated position that weld metal WF-70 is the controlling material.

Ref. 24 contains unirradiated USE data for weld metals designated WF-209-1 A, B, D, and E, but not for C. All of the unirradiated USE values for WF-209-1 weld metal in Ref. 24 are between 64 and 68 ft-lbs. Therefore, the value of 79 ft-lbs. for WF-209-1C (Atypical weld metal) given in Tables 2–1 and 2–2 of Ref. 1 looks high. Of course, the difference in weld wire between Atypical weld metal and the other WF-209-1 welds could account for the difference. WF-209-1 weld metal does not correspond to any of the HSST Linde 80 welds, so a verification of the 79 ft-lb. value given for Atypical weld metal is not possible by comparison with HSST data. The original unirradiated Charpy impact energy data for Atypical weld metal are listed and plotted in Ref. 26. These two sets of data are listed in Tables 6 and 7 and plotted in Fig. 2 of this TER. The highest test temperature was 300°F and the lowest value of USE at that temperature was 79 ft-lobs. Thus 79 ft-lbs. is an appropriate value of unirradiated USE for Atypical weld metal at 300°F. However, assuming an equal value to be applicable at 550°F necessarily requires assuming that the upper shelf portion of the Charpy impact energy curve for Atypical weld metal does not slope downward with increasing temperature between 300°F and 550°F, which sometimes happens. Fortunately, there is archive material from the Atypical weldment still available[26] so that additional Charpy tests can be performed at 550°F if necessary.

There are no J-R data for Atypical weld metal. Thus presently, for the purpose of safety margin evaluation, it is necessary to assume without proof that the Eason correlations are applicable to this material, as has been done in plotting Fig. 1. Assuming the existence of additional archive material,[26] new J-R specimens could be prepared and tested if necessary. Irradiated surveillance specimens of Atypical weld metal may be available, but information beyond that published in the current description of the B&WOG Master Integrated Reactor

Vessel Surveillance Program [28] will be necessary to verify their existence and locate them.

**WF-70 Weld Metal Chemistry**

The copper content used for estimating the J-R curve for WF-70 weld metal, as stated on p. 5-2 of Ref. 1, is 0.35%. This is the value given in Tables 2–1 and 2–2 of Ref. 1. However, the copper content of WF-70 listed in Table 4–2 on p. 4–12 of Ref. 1 is 0.42%, greater than the stated value for Atypical weld metal. This creates confusion concerning the proper value of % Cu to use for WF-70 and about whether Atypical weld metal or WF-70 weld metal should be considered the controlling material. The values of % Cu and % Ni for WF-70 given in Tables 2–1 and 2–2 of Ref. 1 are the same as those given in Ref. 24. However, other values have been reported, some of them recently. Fig. 3, which is Fig. 5 from Ref. 11, shows % Cu for a sample of WF-70 obtained from a weld in nozzle belt dropout varying between 0.34 and 0.45. Fig. 3 directly demonstrated the statistical variability and therefore uncertainty associated with % Cu estimates for weld WF-70. Ref. 11 reports a single test value of % Cu from a reactor vessel surveillance program (RVSP) weld metal qualification test of 0.27, and then a reanalysis value of 0.34. Ref. 11 then goes on to list two different values of % Cu for WF-70 in different tables. Based on Exhibit B4, Table 5 of Ref. 11 states the value of 0.42. Apparently, based on Exhibit B5, which states the value of 0.35 for all welds made with weld wire heat no. 72105, which includes both WF-70 and WF-209-1, Table 6 repeats this value and the corresponding material correctly, but Tables 8 and 10 attribute the value of 0.35 to WF-70 alone. Thus the discrepancy between the % Cu values of 0.42 and 0.35 for WF-70 appears to be due to a change in labeling in Tables 8 and 10 of Ref. 11. Since the only significant difference between Atypical weld metal and WF-70, with regard to their reference J-R curves, is their stated % Cu values, the proper value for WF-70 is important. In notes from the June 13, 1991 meeting between the B&WOG and the NRC,[29] the original values of 0.42 and 0.35 for WF-70 and WW 72105 are given first, followed by revised values of 0.37 and 0.34 respectively. The value of 0.34 is then claimed applicable to WF-70. Clearly, the implicit basis for this claim is the assumption, stated earlier, that Linde 80 flux is "neutral" and, therefore, that the best estimate of % Cu for WF-70 is actually the mean for WW 72105.

Recently a cooperative effort between industry and the NRC has been conducted to obtain chemistry and other data for samples of the WF-70 welds in the canceled Midland plant reactor pressure vessel. ORNL has obtained bulk percent copper contents for two WF-70 Midland welds.[30] The mean % Cu values for a nozzle course weld and a beltline weld are 0.40 and 0.26 respectively. It was concluded by ORNL that these two welds should be considered as two different materials.[30] This conclusion is in definite contrast to the previously stated assumption that % Cu depends on weld wire heat number.

Independently obtained values of % Cu for the Midland welds have been obtained by B&W.[12] Their reported values of % Cu for the nozzle dropout weld, WF-70(N), and the beltline weld, WF-70(B), as given in Table 5–6 of Ref. 12, are 0.40 ± .04 and 0.31 ± .05 respectively. The means of the combined data for the two WF-70 welds is 0.37. Considering both old and new data for WW 72105, the value of % Cu for the old data alone is 0.35 ± .06 and the value for the combined old and new data is 0.34 ± .06 Clearly the WF-209-1 welds, with their lower % Cu, are influencing the average. No physical reason is given in Refs. 30 or 12 for expecting a nozzle course weld to have consistently higher copper content than a beltline weld. Therefore, the occurrence must be considered random.

The reason that the B&W combined average copper content of WF-70 decreased from 0.42% to 0.37% between Refs. 11 and 12 is because of the influence of the Midland Beltline weld which, according to the B&W measurements, has an average copper content of 0.31%. However, the frequency distribution of individual B&W % Cu measurements for WF-70 should have at least two peaks, because the difference between the means for WF-70(N) and WF-70(B) exceeds both the associated standard deviations. Actually, as shown in Fig. 4, the distribution has at least three peaks, again reflecting the complexity and large variability of % Cu values for WF-70 weld metals.

The basic objective of Ref. 12 was to determine whether or not weld metals WF-70 and WF-209-1 exhibit similar behavior with respect to their irradiation induced shifts in $RT_{NDT}$. The "neutrality" of Linde 80 flux was demonstrated, in effect, by the fact that chemistry differences between the two welds, other than copper, are all less than the associated standard deviations. This shift in the temperature associated with a Charpy impact energy of 30 ft-lbs. was found to be independent of copper content. Thus, for licensing purposes, it was recommended that values of Cu = 0.35% and Ni = 0.59% should be used for all WW 72105 weld metals. However, it was found that ΔUSE caused by irradiation does depend on percent copper as well as fluence. Thus differences between the two weld metals with respect to USE were acknowledged. No specific statement was made concerning whether or not the recommended values of % Cu and % Ni are actually considered equally applicable to estimating both ΔUSE and $\Delta RT_{NDT}$. Since this may not be the case, it appears prudent to add some margin to the mean value of % Cu for WW 72105 when calculating the parameters of a J-R curve for irradiated WF-70 weld metal. Adding one standard deviation, .06%, to the more recently calculated mean value of % Cu for WW 72105 given in Ref. 12, 0.34%, gives a value of 0.40%, close to the mean value of Atypical weld metal.

The amount of margin in terms of % Cu above the mean provided in Ref. 1 by overestimating the fluence for the longitudinal WF-70 weld in the Zion 2 vessel can be calculated by considering the term $Cuf^{a_7}$ in the J-R curve estimating correlations. From p. 4–8 and Table 4–4 of Ref. 1, $a_7 = 0.1236$. Let

$$Cu_1 = 0.35\%,$$
$$f_1 = 9.34 \text{ (assumed)},$$

and

$$f_2 = 3.26 \text{ (actual)}.$$

Then

$$Cu_2 f_2^{a_7} = Cu_1 f_1^{a_7}, \tag{1}$$

so that

$$Cu_2 = Cu_2\left(\frac{f_1}{f_2}\right)^{a_7} = 0.40\%. \tag{2}$$

Thus the overestimate of fluence in Ref. 1 has provided both for the possible presence of Atypical weld metal and a prudent margin with respect to the mean value of % Cu for WF-70.

## Comparison of the Eason NRC and B&W Correlations

In the absence of a sufficient number of directly measured J-R curves to cover all relevant conditions, correlations are being used to estimate J-R curves for specified values of fluence, copper content, temperature, and reference specimen size. Two sets of correlations are in use, both developed by Eason et al., one under NRC sponsorship[31] and the other under the sponsorship of B&W Owners Group.[32,33] Ref. 32 describes the B&W correlations based on USE and Ref. 33 describes the B&W correlation based on copper content and fluence, acting together as the product $a_2Cuf^{a_7}$, where $a_2$ is an empirical constant, Cu is percent copper, f is fluence × $10^{-18}$ n/cm$^2$ and $a_7$ is an empirical exponent. Using a value of % Cu - 0.26 relevant to a previous analysis for Turkey Point,[34] calculations were made to compare the NRC and B&W mean value correlations for a range of fluences, temperatures and reference specimens sizes. As illustrated in Fig. 5, for % Cu = 0.26 the rate of increase of $J_{0.1}$ with $B_N$ is consistently less for the NRC correlation than for the B&W correlation. At some value of $B_N$ a crossover occurs beyond which the B&W correlation estimates higher values of $J_{0.1}$ than the NRC correlation. The value of $B_N$ at the crossover progressively decreases with increasing temperature and increasing fluence. At 550°F, the unirradiated crossover occurs at $B_N = 1.0$ in. At $\Phi = 2 \times 10^{19}$ n/cm$^2$, the crossover occurs at $B_N = 0.2$ in. Additional calculations show that the crossover values and patters can be different at the $-2\sigma$ level of $J_{0.1}$ and for other values of % Cu.

Calculated J-R curves for the Zion vessels, based on both the Eason-NRC and the Eason B&W correlations, and the applied J-R curves from Ref. 1 lead to the results shown in Figs. 6 and 7. For the copper content of WF-70 stated in Ref. 1, 0.35%, Fig. 6 shows, somewhat unexpectedly, that at a quarter-thickness fluence of $3.26 \times 10^{18}$ n/cm$^2$, the NRC correlation estimates a considerably higher J-R curve than the B&W correlation. But for a quarter-thickness fluence of $9.34 \times 10^{18}$ n/cm$^2$, the two curves are very close, with the NRC curve being slightly lower. Both criteria #1 and #2 for Level A and B are satisfied. For the copper content of Atypical weld metal, 0.41%, and quarter-thickness fluence of $3.26 \times 10^{18}$ n/cm$^2$, Fig. 7 shows that the J-R curve estimated by the B&W correlation is considerably lower than that estimated by the NRC correlation and that both Criteria #1 and #2 are satisfied. Furthermore, the lowest (B&W) curve in Fig. 7 for % Cu = 0.41 and f = 3.26 nearly coincides with the two lowest (B&W and NRC) curves in Fig. 6 for % Cu - 0.35 and f - 9.34. Thus the B&W correlation appears to provide considerably better compensation for the possible presence of Atypical weld metal and variability of copper content in WF-70, for Level A and B loads, than the NRC correlation. However, Fig. 8 shows that this comparison depends on fluence. The B&W correlation predicts a steeper initial decline in tearing resistance with increasing fluence than the NRC correlation for % Cu = 0.35. However, a reversal occurs near the peak quarter-thickness circumferential fluence in the Zion vessels, so that for fluences exceeding $10^{19}$ n/cm$^2$, the NRC correlation would be more conservative. Fig. 9 shows that at a fluence of $10^{19}$ n/cm$^2$, the NRC correlation is more conservative than the B&W correlation for $0.26 < \%\ Cu < 0.41$. Thus the conservatism of the B&W correlation is somewhat specific to the quarter-thickness fluence range in the Zion vessels.

**Review of Analysis**

In Principle, the calculations of stress intensity factors and applied J values for a low-upper-shelf safety margin analysis for Levels A and B are straightforward, especially when following Code Case N-512 (Ref. 7). Nevertheless, checking is important because the LUS safety margin issue may govern permissible plant life, calculated safety margins may be close to the specified minimum values, and because some choices in the calculations are not completely specified by Code Case N-512. In particular, the following aspects of the calculations in Ref. 1 were noted and examined: consideration of cladding thickness, omission of the plastic zone size correction to the thermal component of $K_I$, accuracy and consistency in choices of elastic modulus, and J-R curve plotting accuracy.

The thickness of the cladding,[35] 0.156 in., was not included in the vessel wall thickness for calculating $K_{Ip}$, although cladding thickness was supposedly considered in the calculations leading to $K_{It} = 21$ ksi$\sqrt{in.}$ taken from Ref. 35. These choices are, by themselves, conservative and accurate, respectively. Furthermore, the net effect on the total $K_I$ value for values calculated according to Code Case N-512 is less than 1%. The total vessel wall thickness considered in Ref. 35 was 8.9 in., greater than the total thickness of a Zion vessel. Nevertheless, the value of $K_{It} = 21$ ksi$\sqrt{in.}$ from Ref. 35 is slightly less than the value of 23 ksi$\sqrt{in.}$ calculated for a Zion vessel according to Code Case N-512, including the effect of cladding thickness. The difference between the two values of $K_{It}$ is probably due to either neglecting the cladding thickness is the stress analysis in Ref. 35 (no statement is made on this subject in Ref. 35) or to differences in assumed material properties, principally thermal diffusivity. Since the difference in $K_{It}$ values is less than 2% of the total elastically calculated value of $K_I$, it was not pursued further.

As can be seen from the equation for J at the top of p. 5–4 in Ref. 1, no plastic zone size correction was applied to the value of $K_{It}$. The effect of this omission is to lower the calculated value of J at a = t/4 + 0.10 in. for criterion #1 from 509 to 487.5 in-lbs./in.[2] This procedure is not consistent with Code Case N-512 (Ref. 7) nor is it consistent with the Level C and D calculations performed by B&W for the generic analysis of 16 B&WOG plants.[36] No physical justification is given for this omission. Absent this justification, it is considered unconservative. Nevertheless, calculations made by the writers, to be discussed below, show that Code Case N-512 criteria are still satisfied, albeit with little or no excess margin, when plastic zone size effects are considered.

The value of the elastic modulus used to convert $K_I$ to J in Ref. 1 was a value calculated from the equation

$$E = (29.8 - 0.00533T) \times 10^6 \text{ PSI} \tag{3}$$

For T = 530°F, E = 26.975 × $10^6$ PSI. The chosen temperature of 530°F is the Zion vessels' coolant inlet temperature and, therefore, a close approximation to the downcomer region and vessel wall temperatures during normal operation. However, according to B&W's response to the Zion Request for Additional Information (RAI),[37] the value of E calculated in Ref. 1 does not agree with the value of E at the same temperature used to calculate the value of $K_{It} = 21$ ksi$\sqrt{in.}$ obtained from Ref. 35. Thus it was necessary to trace the sources of data upon which the values of E in Refs. 1 and 37 were based.

Ref. 1 states the Eq. 3 was obtained from its Ref. 45, which is the B&W calculation document for Zion.[38] This document refers to the B&W calculation document for Turkey Point[39] which states that Eq. 3 fits data (for Carbon-molybdenum steels) in Appendix I, Section III of the ASME Code, 1983.[40] These data are shown boxed in Table 8. While Eq. 3 does calculate the value of E at 530°F given in Ref. 1, it does not exactly calculate the values given in Table 8 as claimed in Ref. 39. A constant of 29.6 in Eq. 3 gives results much closer to those in Table 8 than 29.8. This error is small, but hard to explain. The values of E in Table 8 which were supposed to have been fit by Eq. 3, remain unchanged up to the current 1992 edition of Section II of the code,[41] as shown in Table 9. However, there is some question as to whether A533-B should be assigned to Group A or Group B in Table 9 because of its nickel content. Nevertheless, because differences between Groups A and B are small and the material of concern is a weld rather than base metal, this issue will not be pursued here.

In B&W's reply to the Zion RAI[37] it was shown that the calculations of $K_{It}$ in Ref. 35 were supposed to have been based on properties obtained from the 1986 edition of Section III of the ASME Code. Thus the values should have been identical to those upon which Eq. 3 was based. However, the values given in Ref. 37 shown in Table 10 do not match the values in the 196 edition of the code, which are the same as those given in Tables 8 and 9. However, they do match older values given in the 1977 edition of Section III, Div. 1, Subsection NA, Appendix I,[42] as shown by the boxed values in Table 11. Thus the values of E used for B&W's calculations of $K_{It}$ in Ref. 35 were out-of-date, on the high side, apparently due to oversight. Again the error appears to be small. The change of modulus values in the ASME Code appears to have taken place in the 1980 edition.

In examining Fig. 5–13 of Ref. 1, it appeared that the plotted point for J applied at $\Delta a$ - 0.10 in. for criterion #1 does not correspond to the state value of J - 487.5 in-lbs./in.[2] Scaling from an expanded copy of the figure, as shown in Fig. 10, the plotted point corresponds to J = 475 in-lbs./in.,[2] thus making the margin look bigger than it is. Considering all the matters of accuracy discussed above, it was deemed prudent to perform some independent checking and sensitivity calculations for the Zion vessels.

Because of the known effects of temperature on both the J-R curve and the elastic modulus, and the existence of two sets of J-R curve estimating correlations, two graphical comparisons between applied J values and the J-R curves were prepared, one for a temperature of 550°F and the other for a temperature of 530°F. All the calculations of applied J are for the deepest point of the crack, assuming, as does Code Case N-512, that this is the governing point for the calculation of LUS safety margins. The general validity of this assumption, especially for Level C and D loads (not at issue here) remains to be established. The calculations of applied J values were performed according to Code Case N-512, therefore including plastic zone size corrections to both $K_{Ip}$ and $K_{Ip}$. Estimates of elastic modulus values were made on the basis of a least squares linear fit to the modulus data between 200°F and 600°F for Ferrous Materials, Group A, in Table 9, the same data used by B&W. The resulting equation is

$$E = [29.54 - 0.0052T] \times 10^6, \text{ PSI.} \tag{4}$$

Eq. 4 leads to elastic modulus values of $26.784 \times 10^6$, PSI at 530°F and $26.680 \times 10^6$, PSI at 550°F, slightly less than the values given by Eq. 3. Three J-R curves for $B_N = 0.8$ in. were used for comparison with the applied J curves. The first curve was the Eason - B&W curve for % Cu = 0.41 and f = 3.26, and the second curve was the Eason - B&W curve for % Cu - 0.35 and f = 9.34. The third curve was the Eason - NRC curve for % Cu - 0.35 and f = 9.34. These three curves were known to be very close to each other, as discussed previously. For a temperature of 530°F, Fig. 11 shows that both Level A and B Criteria #1 and #2 are satisfied. For a temperature of 550°F, Fig. 12 shows that Level A and B Criterion #2 is failed for all three J-R curves, and Level A and B Criterion #1 is failed for the Eason - NRC J-R curve. This is primarily because of the temperature sensitivity of the J-R curves between 530°F and 550°F, as illustrated further for $J_{0.1}$ in Figs. 13 and 14. The increase in the applied J values between the two temperatures due to a decreasing elastic modulus is slight. Of course, these results pertain to the chosen net section thickness, $B_N = 0.8$ in. Increasing the value of $B_N$, if that can be justified, would increase the calculated safety margins. For example, Ref. 16 suggests $B_N = 1.0$ in. The purpose of including Fig. 12 for a temperature of 550°F is not to claim that a temperature is higher than 530°F should be used for the governing calculations for the Zion vessels, but rather to illustrate the sensitivity of the resulting calculated LUS safety margins to temperature. It follows that for regulatory purposes it is very important to verify that 530°F is the correct reference temperature to use for calculating the LUS safety margins for the Zion vessels.

# CONCLUSIONS

## Technical Conclusions

1. Providing that the reference temperature of 530°F is actually the vessel wall temperature at normal operation, the Zion vessels satisfy both Criteria #1 and #2 of Code Case N-512 for Level A and B loadings. However, if the reference temperature were 550°F, that would not be the case.

2. The equivalence of safety margins between upper shelf and transition range conditions is basically qualitative in the sense that in the best judgement of ASME Code personnel, the specified safety margins and criteria in Appendix G and Code Case N-512 are both equally appropriate and adequate.

3. Both the variability of copper contents for WF-70 welds and the possible presence of Atypical weld metal in the Zion vessels have been indirectly considered in the analyses of Ref. 1 by overestimating the fluence for the longitudinal WF-70 welds.

4. The omission of cladding thickness in the analyses of Ref. 1 has only a minor effect.

5. The omission of the plastic zone size correction to the thermal component of $K_I$ in Ref. 1 is unconservative and not in accordance with Code Case N-512. Nevertheless, calculations including plastic zone size effects made by the writers show that Code Case N-512 criteria are still satisfied, albeit with little or no excess margins.

6. The elastic modulus values used in the calculations of Ref. 1 were inconsistent and contained small errors.

7. Because calculated LUS safety margins for the Zion vessels are near the minimum specified by Code Case N-512, the variabilities of these margins with respect to almost all of the input parameters are significant.

8. The utility's submittal reviewed herein was based on fluence values since revised, slightly upward (see Appendix A). Considering reasonable limits of calculational accuracy and NRC's current recommendations regarding assumed specimen size for estimating J-R curves, Code Case N-512 criteria are still judged to be satisfied, but the margin for criterion #1 is near or at its minimum acceptable value.

## Regulatory Conclusions

The Zion Units 1 and 2 reactor pressure vessels have been shown to have adequate margins of safety against ductile tearing in low-upper-shelf longitudinal welds, at presently projected end-of-life, for Level A and B conditions, by analysis results meeting the criteria contained in ASME Code Case N-512. The adequacy of the calculated LUS safety margins for the Zion vessels depends upon the correctness of the assumed reference temperature, 530°F.

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## APPENDIX A. EFFECT OF REVISION TO FLUENCE ESTIMATES FOR THE ZION VESSELS.

After completion of the first draft of this document and during the preparation of the draft Level C and D TER for the combined set of 16 B&WOG vessels, it was discovered that the report upon which the fluence values given in Ref. 1 were based was not the latest revision available at the time Ref. 1 was issued. Ref. 1 used fluence values from its Ref. 23, which was WCAP-10962, Rev. 2, dated December 1990.[43] B&WOG's Level C and D analysis of the 16 plants[44] used fluence values for the Zion vessels obtained from Rev. 3 of WCAP-10962, dated September 1991.[45] Since Ref. 45 predates Ref. 1 by six months, it is not clear why it was not used in place of Ref. 43 in Ref. 1. Because the fluence values given in Ref. 45 are slightly higher than those given in Ref. 43, as indicated by the B&WOG Level C and D RAI response for the 16 plants, it because necessary to replot Fig. 11 to determine if the Zion vessels still satisfy the Level A and B criteria in Code Case N-512.

Fig. A1, which indicates the revised fluence values at $t/4$ in the legend, shows that criterion #2, the stability criterion, is just satisfied for all three estimated $-2\sigma$ J-R curves. For criterion #1, the limited crack growth criterion, the B&W correlation $-2\sigma$ J-R curves both lie above the applied J curve at $\Delta a = 0.10$ in. However, the NRC correlation $-2\sigma$ J-R curve intersects the applied J curve just beyond $\Delta a = 0.10$ in. The difference between the two values of J at exactly $\Delta a = 0.10$ in. is extremely small, far less than the accuracy of the calculations. Considering in addition the fact that Ref. 16 recommends using a net section thickness of 1.0 in. with the NRC correlation instead of the more conservative value of 0.8 in. chosen in Ref. 1, criterion #1 is still considered satisfied, based on the NRC correlation, but with no excess margin. Based on the B&W correlation curves, a small amount of excess margin still exists.

**Table 1**

Plants with reactor vessel upper shelf energies currently below 50 ft–lbs based on the NRC staff generic guidance:

Nine Mile Point I
Oyster Creek I
Arkansas Nuclear One–1
Crystal River 3
Ginna
Oconee 1
Oconee 2
Point Beach 1
Point Beach 2
Robinson 2
Three Mile Island 1
Turkey Point 3
Turkey Point 4
Zion 1
Zion 2

**Table 2**

Plants with reactor vessel upper shelf energies less than 50 ft–lbs before the end of their operating license based on the NRC staff generic guidance:

Oconee 3
Millstone 2
Watts Bar 1

**Table 3**

Low-Upper-Shelf Safety Margin Analyses Begun by Utilities Before February 25, 1993.

Turkey Point Units 3 and 4
Zion Units 1 and 2
Babcock and Wilcox Owners Group
Nine Mile Point Unit 1
Oyster Creek

**Table 4**

Atypical Weld Chemistry, Not Including Copper

| | C | Mn | P | S | Si | Cr | Ni | Mo |
|---|---|---|---|---|---|---|---|---|
| CR-3 Weld | .08 | 1.65 | .021 | .013 | 1.0 | .07 | .10 | .45 |
| Mn-Mo-Ni (Typical) | .08 | 1.6 | .018 | .015 | .5 | .07 | .60 | .40 |

**Table 5**

Location of Possible Atypical Welds

| Plant | Location of Weld |
|---|---|
| B&W | |
| Oconee 3 | Center Circ. Beltline |
| TMI 1 | Upper Circ. Beltline |
| | Lower Circ. Beltline |
| TMI 2 | Dutchman to Lowerhead |
| ANO 1 | Head to Flange and Nozzel to Shell |
| Midland 1 | Center Circ. Beltline |
| CR-3 | Center Circ. Beltline |
| Rancho Seco | Vertical Seam Beltline |
| Westinghouse | |
| Zion 1 | Inter to Lower Circ. Beltline |
| Zion 2 | Vertical Seam Beltline (0 and 180°) |
| Turkey Pt. 4 | Nozzel Shell to Interm. Circ. |
| GE | |
| Br. Ferry 1 | Shell to Flange and Longitudinal Weld in Beltline |
| Quad Cities 2 | Closure Head to Flange |

Table 6. Pre-Irradiation Charpy Impact Data for Atypical Weld Metal Tested at Mt. Vernon

| Test temp. F | Absorbed energy, ft-lb. | Lateral expans., $10^{-3}$ in. | Shear fracture. % |
|---|---|---|---|
| 10 | 15 | 6 | 15 |
| | 16 | 7 | 15 |
| | 18 | 10 | 15 |
| 70 | 35 | 25 | 35 |
| | 36 | 24 | 35 |
| | 26 | 18 | 25 |
| 100 | 39 | 22 | 40 |
| | 46 | 30 | 25 |
| | 26 | 18 | 25 |
| 150 | 65 | 47 | 85 |
| | 74 | 58 | 100 |
| | 38 | 28 | 60 |
| | 48 | 32 | 65 |
| | 68 | 52 | 85 |
| 170 | 44 | 35 | 65 |
| | 70 | 59 | 100 |
| | 70 | 61 | 100 |
| | 38 | 27 | 65 |
| | 63 | 48 | 90 |
| 200 | 76 | 57 | 100 |
| | 66 | 48 | 90 |
| | 78 | 60 | 100 |
| 300 | 79 | 60 | 100 |
| | 81 | 63 | 100 |
| | 84 | 62 | 100 |

Table 7. Pre-Irradiation Charpy Impact Data for Atypical Weld Metal

| Specimen No. | Test temp., F | Absorbed energy, ft-lb | Lateral expans., $10^{-3}$ in. | Shear fracture, % |
|---|---|---|---|---|
| PP047 | 0 | 21 | 20 | 20 |
| PP050 | 15 | 27 | 26 | 30 |
| PP030 | 30 | 31 | 34 | 30 |
| PP042 | 40 | 34 | 32 | 30 |
| PP023 | 55 | 30 | 30 | 30 |
| PP027 | 75 | 36 | 36 | 40 |
| PP048 | 90 | 44 | 46 | 100 |
| PP040 | 105 | 46 | 45 | 100 |
| PP009 | 120 | 56 | 63 | 100 |
| PP012 | 135 | 39 | 42 | 100 |
| PP013 | 150 | 63 | 72 | 100 |
| PP025 | 200 | 68 | 69 | 100 |
| PP045 | 230 | 78 | 77 | 100 |
| PP020 | 260 | 79 | 77 | 100 |
| PP019 | 300 | 79 | 78 | 100 |

## TABLE I-6.0
## MODULI OF ELASTICITY $E$ OF MATERIALS FOR GIVEN TEMPERATURES

| Material | Modulus of Elasticity $E$ = Value Given $\times 10^6$ psi, for Temp. °F of | | | | | | | | | | |
|---|---|---|---|---|---|---|---|---|---|---|---|
| | -325 | -200 | -100 | 70 | 200 | 300 | 400 | 500 | 600 | 700 | 800 |
| **Ferrous Materials** | | | | | | | | | | | |
| Carbon Steels with $C \leq 0.30\%$ | 31.4 | 30.8 | 30.2 | 29.5 | 28.8 | 28.3 | 27.7 | 27.3 | 26.7 | 25.5 | 24.2 |
| Carbon steels with $C > 0.30\%$ | 31.2 | 30.6 | 30.0 | 29.3 | 28.6 | 28.1 | 27.5 | 27.1 | 26.5 | 25.3 | 24.0 |
| Carbon–molybdenum steels | 31.1 | 30.5 | 29.9 | 29.2 | 28.5 | 28.0 | 27.4 | 27.0 | 26.4 | 25.3 | 23.9 |
| Nickel steels | 29.6 | 29.1 | 28.5 | 27.8 | 27.1 | 26.7 | 26.1 | 25.7 | 25.2 | 24.6 | 23.0 |
| Chrome–molybdenum steels | | | | | | | | | | | |
| $\frac{1}{2}$–2 Cr | 31.6 | 31.0 | 30.4 | 29.7 | 29.0 | 28.5 | 27.9 | 27.5 | 26.9 | 26.3 | 25.5 |
| $2\frac{1}{4}$–3 Cr | 32.6 | 32.0 | 31.4 | 30.6 | 29.8 | 29.4 | 28.8 | 28.3 | 27.7 | 27.1 | 26.3 |
| 5–9 Cr | 32.9 | 32.3 | 31.7 | 30.9 | 30.1 | 29.7 | 29.0 | 28.6 | 28.0 | 27.3 | 26.1 |
| Straight chromium steels | 31.2 | 30.7 | 30.1 | 29.2 | 28.5 | 27.9 | 27.3 | 26.7 | 26.1 | 25.6 | 24.7 |
| Austenitic, precipitation hardened, and other high alloy steels | 30.3 | 29.7 | 29.1 | 28.3 | 27.6 | 27.0 | 26.5 | 25.8 | 25.3 | 24.8 | 24.1 |
| **Nonferrous Materials** | | | | | | | | | | | |
| High Nickel Alloys | | | | | | | | | | | |
| N02200 (200), N02201 (201) | 32.1 | 31.5 | 30.9 | 30.0 | 29.3 | 28.8 | 28.5 | 28.1 | 27.8 | 27.3 | 26.7 |
| N04400 (400), N04405 (405) | 27.8 | 27.3 | 26.8 | 26.0 | 25.4 | 25.0 | 24.7 | 24.3 | 24.1 | 23.7 | 23.1 |
| N07750 (750) | 33.2 | 32.6 | 31.9 | 31.0 | 30.2 | 29.8 | 29.5 | 29.0 | 28.7 | 28.2 | 27.6 |
| N07718 (718) | 31.0 | 30.5 | 29.9 | 29.0 | 28.3 | 27.8 | 27.6 | 27.1 | 26.8 | 26.4 | 25.8 |
| N06002 (X) | 30.5 | 29.9 | 29.4 | 28.5 | 27.8 | 27.4 | 27.1 | 26.6 | 26.4 | 25.9 | 25.4 |
| N06600 (600) | 33.2 | 32.6 | 31.9 | 31.0 | 30.2 | 29.9 | 29.5 | 29.0 | 28.7 | 28.2 | 27.6 |
| N06625 (625) | 32.1 | 31.5 | 30.9 | 30.0 | 29.3 | 28.8 | 28.5 | 28.1 | 27.8 | 27.3 | 26.7 |
| N08020 (20Cb-3) | 30.0 | 29.4 | 28.8 | 28.0 | 27.3 | 26.9 | 26.6 | 26.2 | 25.9 | 25.5 | 24.9 |
| N08800 (800), N08810 (800H) | 30.5 | 29.9 | 29.4 | 28.5 | 27.8 | 27.4 | 27.1 | 26.6 | 26.4 | 25.9 | 25.4 |
| N08825 (825) | 30.0 | 29.4 | 28.8 | 28.0 | 27.3 | 26.9 | 26.6 | 26.2 | 25.9 | 25.5 | 24.9 |
| N10001 (B) | 33.3 | 32.7 | 32.0 | 31.1 | 30.3 | 29.9 | 29.5 | 29.1 | 28.8 | 28.3 | 27.7 |
| **Aluminum and Aluminum Alloys** | | | | | | | | | | | |
| A03560 (356), A95083 (5083), A95086 (5086), A95456 (5456) | 11.4 | 11.1 | 10.8 | 10.3 | 9.8 | 9.5 | 9.0 | 8.1 | ... | ... | ... |



TABLE 8

MATERIALS; PART D, PROPERTIES



## TABLE TM-1
## MODULI OF ELASTICITY *E* OF FERROUS MATERIALS FOR GIVEN TEMPERATURES

| Materials | Modulus of Elasticity $E$ = Value Given × $10^6$ psi, for Temp., °F, of | | | | | | | | | | | |
|---|---|---|---|---|---|---|---|---|---|---|---|---|
| | −325 | −200 | −100 | 70 | 200 | 300 | 400 | 500 | 600 | 700 | 800 | 900 |
| Carbon steels with C ≤ 0.30% | 31.4 | 30.8 | 30.2 | 29.5 | 28.8 | 28.3 | 27.7 | 27.3 | 26.7 | 25.5 | 24.2 | 22.4 |
| Carbon steels with C > 0.30% | 31.2 | 30.6 | 30.0 | 29.3 | 28.6 | 28.1 | 27.5 | 27.1 | 26.5 | 25.3 | 24.0 | 22.3 |
| Material Group A[1] | 31.1 | 30.5 | 29.9 | 29.2 | 28.5 | 28.0 | 27.4 | 27.0 | 26.4 | 25.3 | 23.9 | 22.2 |
| Material Group B[2] | 29.6 | 29.1 | 28.5 | 27.8 | 27.1 | 26.7 | 26.1 | 25.7 | 25.2 | 24.6 | 23.0 | . . . |
| Material Group C[3] | 31.6 | 31.0 | 30.4 | 29.7 | 29.0 | 28.5 | 27.9 | 27.5 | 26.9 | 26.3 | 25.5 | 24.8 |
| Material Group D[4] | 32.6 | 32.0 | 31.4 | 30.6 | 29.8 | 29.4 | 28.8 | 28.3 | 27.7 | 27.1 | 26.3 | 25.6 |
| Material Group E[5] | 32.9 | 32.3 | 31.7 | 30.9 | 30.1 | 29.7 | 29.0 | 28.6 | 28.0 | 27.3 | 26.1 | 24.7 |
| Material Group F[6] | 31.2 | 30.7 | 30.1 | 29.2 | 28.5 | 27.9 | 27.3 | 26.7 | 26.1 | 25.6 | 24.7 | 23.2 |
| Material Group G[7] | 30.3 | 29.7 | 29.1 | 28.3 | 27.6 | 27.0 | 26.5 | 25.8 | 25.3 | 24.8 | 24.1 | 23.5 |

NOTES:

(1) Material Group A consists of the following carbon–molybdenum steels:

| | |
|---|---|
| C–1/2Mo | Mn–1/4Mo |
| Mn–1/2Mo | Mn–V |

(2) Material Group B consists of the following Ni steels:

| | |
|---|---|
| 3/4Ni–1/2Mo–Cr–V | 1Ni–1/2Cr–1/2Mo |
| 1/2Ni–1/2Mo–V | 3/4Ni–1Mo–3/4Cr |
| 3/4Ni–1/2Mo–1/3Cr–V | 3/4Ni–1/2Cr–1/2Mo–V |
| 3/4Cr–3/4Ni–Cu–Al | 2Ni–1Cu |
| 1/2Cr–1/2Ni–Cu | 2 1/2Ni |
| 3/4Ni–1/2Cu–Mo | 3 1/2Ni |

(3) Material Group C consists of the following 1/2–2Cr steels:

1/2Cr–1/2Mo
1Cr–1/2Mo
1 1/4Cr–1/2Mo–Si
1 1/4Cr–1/2Mo
2Cr–1/2Mo

(4) Material Group D consists of the following 2 1/4–3Cr steels:

2 1/4Cr–1Mo
3Cr–1Mo

(5) Material Group E consists of the following 5–9Cr steels:

5Cr–1/2Mo
5Cr–1/2Mo–Si
5Cr–1/2Mo–Ti
7Cr–1/2Mo
9Cr–Mo

(6) Material Group F consists of the following chromium steels:

12Cr–Al
13Cr
15Cr
17Cr

(7) Material Group G consists of the following austenitic steels:

| | |
|---|---|
| 18Cr–8Ni | 18Cr–10Ni–Cb |
| 18Cr–8Ni–N | 18Cr–18Ni–2Si |
| 16Cr–12Ni | 20Cr–6Ni–9Mn |
| 18Cr–13Ni–3Mo | 22Cr–13Ni–5Mn |
| 16Cr–12Ni–2Mo–N | 23Cr–12Ni |
| 18Cr–3Ni–13Mn | 25Cr–20Ni |
| 18Cr–10Ni–Ti | |



TABLE 9

Table 1 Input Material Properties

The following is a list of temperature dependent material properties:

| TEMP | E | ALPHA | K | CP | RHO | POISSONS RATIO |
|---|---|---|---|---|---|---|
| 10., | 29.989, | 7., | 286.6, | .097, | 491.7, | .3 |
| 70., | 29.899, | 7., | 278.4, | .104, | 490.9, | .3 |
| 100., | 29.828, | 7.01, | 275.1, | .107, | 490.5, | .3 |
| 150., | 29.681, | 7.05, | 270.8, | .111, | 489.9, | .3 |
| 200., | 29.503, | 7.10, | 267.6, | .115, | 489.2, | .3 |
| 250., | 29.301, | 7.15, | 265.3, | .118, | 488.6, | .3 |
| 300., | 29.082, | 7.21, | 263.7, | .12, | 487.9, | .3 |
| 350., | 28.847, | 7.25, | 262.5, | .123, | 487.3, | .3 |
| 400., | 28.597, | 7.3, | 261.6, | .125, | 486.7, | .3 |
| 450., | 28.331, | 7.34, | 260.6, | .126, | 486., | .3 |
| 500., | 28.048, | 7.39, | 259.5, | .128, | 485.4, | .3 |
| 550., | 27.74, | 7.44, | 257.8, | .130, | 484.7, | .3 |
| 600., | 27.403, | 7.5, | 255.6, | .133, | 484.1, | .3 |
| 650., | 27.026, | 7.58, | 252.5, | .135, | 483.4, | .3 |
| 700., | 26.599, | 7.70, | 248.4, | .139, | 482.8, | .3 |

where

| | | |
|---|---|---|
| TEMP | = | temperature, F |
| E | = | Young's modulus, $10^6$ psi |
| Alpha | = | thermal expansion coefficinet, $10^{-6}$ in/in |
| K | = | thermal conductivity, Btu/in-hr-F |
| CP | = | specific heat, Btu/lbm-F |
| Rho | = | density, lbm/$ft^3$ |
| TD | = | thermal diffusivity |

$$TD = K/(Rho \times CP)$$

TABLE 10

TABLE I-6.0
MODULI OF ELASTICITY OF MATERIALS FOR GIVEN TEMPERATURES

| Material | Modulus of Elasticity, $E$, = Value Given × $10^6$ (psi) for Temperature (F) of | | | | | | | | | | |
|---|---|---|---|---|---|---|---|---|---|---|---|
| | −325 | −200 | −100 | 70 | 200 | 300 | 400 | 500 | 600 | 700 | 800 |
| Ferrous Materials | | | | | | | | | | | |
| Carbon steels with carbon content 0.30 or less, 3½ Ni | 30.0 | 29.5 | 29.0 | 27.9 | 27.7 | 27.4 | 27.0 | 26.4 | 25.7 | 24.8 | ... |
| Carbon steels with carbon content above 0.30 | 31.0 | 30.6 | 30.4 | 29.9 | 29.5 | 29.0 | 28.3 | 27.4 | 26.7 | 25.4 | ... |
| Carbon-molybdenum steels, low chrome steels through 3 Cr | 31.0 | 30.6 | 30.4 | 29.9 | 29.5 | 29.0 | 28.6 | 28.0 | 27.4 | 26.6 | ... |
| Intermediate chrome steels (5 Cr through −9 Cr) | 29.4 | 28.5 | 28.1 | 27.4 | 27.1 | 26.8 | 26.4 | 26.0 | 25.4 | 24.9 | ... |
| Austenitic steels (304,310,316,321,347) | 30.4 | 29.9 | 29.4 | 28.3 | 27.7 | 27.1 | 26.6 | 26.1 | 25.4 | 24.8 | 24.1 |
| Straight chromium steels (12 Cr, 17 Cr, 27 Cr) | 30.8 | 30.3 | 29.8 | 29.2 | 28.7 | 28.3 | 27.7 | 27.0 | 26.0 | 24.8 | 23.1 |
| Nonferrous Materials | | | | | | | | | | | |
| High Nickel Alloys | | | | | | | | | | | |
| Ni-Cr-Fe, Ni-Fe-Cr, Ni-Cu | | | ... | 31.7 | 30.9 | 30.5 | 30.0 | 29.6 | 29.2 | 28.6 | 27.9 |
| Ni-Cr-Fe-Mo-Cb | | | ... | 29.0 | 28.4 | 27.9 | 27.5 | 27.1 | 26.7 | 26.3 | 25.8 |
| Aluminum Alloys[1] | | | | | | | | | | | |
| 3003 | 11.1 | ... | 10.4 | 10.0 | 9.6 | 9.1 | 8.3 | ... | ... | ... | ... |
| 3004 | ... | ... | ... | 10.0 | 9.6 | 9.1 | 8.3 | ... | ... | ... | ... |
| 5052, 5154 | 11.3 | ... | 10.6 | 10.2 | 9.8 | 9.0 | 8.0 | ... | ... | ... | ... |
| 5454, 5456 | ... | ... | ... | 10.2 | 9.8 | 9.0 | 8.0 | ... | ... | ... | ... |
| 5083, 5086 | 11.4 | ... | 10.7 | 10.3 | 10.0 | 9.5 | 8.7 | ... | ... | ... | ... |
| 6061 | 11.1 | ... | 10.4 | 10.0 | 9.6 | 9.2 | 8.7 | ... | ... | ... | ... |
| 6063 | ... | ... | ... | 10.0 | 9.6 | 9.1 | 8.3 | ... | ... | ... | ... |
| 2014, 2024 | 11.8 | ... | 11.0 | 10.5 | 10.3 | 9.9 | 9.2 | ... | ... | ... | ... |
| Other Nonferrous Materials | | | | | | | | | | | |
| Copper Nickel (70-30) | | | | 22.0 | ... | ... | ... | ... | ... | ... | ... |
| Copper | 17.0 | 16.7 | 16.5 | 16.0 | 15.6 | 15.4 | 15.1 | 14.7 | 14.2 | 13.7 | ... |
| Unalloyed Titanium | ... | | | 15.5 | 15.0 | | 13.8 | 13.2 | 12.5 | 11.8 | 11.2 |

S77 NOTE:
(1) These are typical modulus values, not guaranteed or specified.



TABLE 11

# FIGURE CAPTIONS

Fig. 1 Mean and $-2\sigma$ J-R curves estimated by the Eason - B&W correlation for WF-70 weld metal at fluences of $3.26 \times 10^{18}$ and $9.34 \times 10^{18}$ n/cm$^2$, and for Atypical weld metal at a fluence of $3.26 \times 10^{18}$ n/cm$^2$; T = 550°F, $B_N = 0.8$ in.

Fig. 2 Charpy impact data for unirradiated Atypical weld metal.

Fig. 3 Through-thickness copper variability for a sample of WF-70 obtained from a weld in a nozzle belt dropout (Fig. 5 from Ref. 11).

Fig. 4 Frequency distribution of individual B&W copper content measurements for WF-70.

Fig. 5 Comparison of Eason-NRC and Eason-B&W mean toughness values at 0.1 in. crack extension ($J_{0.1}$) for a range of fluences, temperatures and net-section thicknesses based on a common copper content of 0.26 wt %.

Fig. 6 Comparison of calculated $-2\sigma$ J-R curves, based on Eason-B&W and Eason-NRC correlations, and the $J_{applied}$ curves from Ref. 1. The J-R curves are based on copper content of WF-70 (0.35 wt %), axial ($3.26 \times 10^{18}$ n/cm$^2$) or circumferential ($9.34 \times 10^{18}$ n/cm$^2$) fluence, reference temperature of 550°F and net-section thickness of 0.8 in.

Fig. 7 Comparison of calculated $-2\sigma$ J-R curves, based on Eason-B&W and Eason-NRC correlations, and the $J_{applied}$ curves from Ref. 1. The J-R curves are based on copper content of Atypical Weld (0.41 wt %), axial fluence of $3.26 \times 10^{18}$ n/cm$^2$, reference temperature of 550°F and net-section thickness of 0.8 in.

Fig. 8 Comparison of fluence sensitivity between Eason-B&W and Eason-NRC correlations relative to unirradiated (f=0) values. Comparison is based on toughness values for 0.1 in. crack extension ($J_{0.1}$), copper content of WF-70 (0.35 wt %), reference temperature of 550°F and net section thickness of 0.8 in.

Fig. 9 Comparison of combined fluence and copper-content sensitivity between Eason-B&W and Eason-NRC correlations relative to unirradiated (f=0) values. Comparison is based on toughness values for 0.1 in. crack extension ($J_{0.1}$), fluence of $1 \times 10^{19}$ n/cm$^2$, reference temperature of 550°F and net-section thickness of 0.8 in.

Fig. 10 Expanded figure from Ref. 1 showing calculated $-2\sigma$ J-R curves and $J_{applied}$ curves for criterion #1; figure used to examine plotting accuracy in Ref. 1.

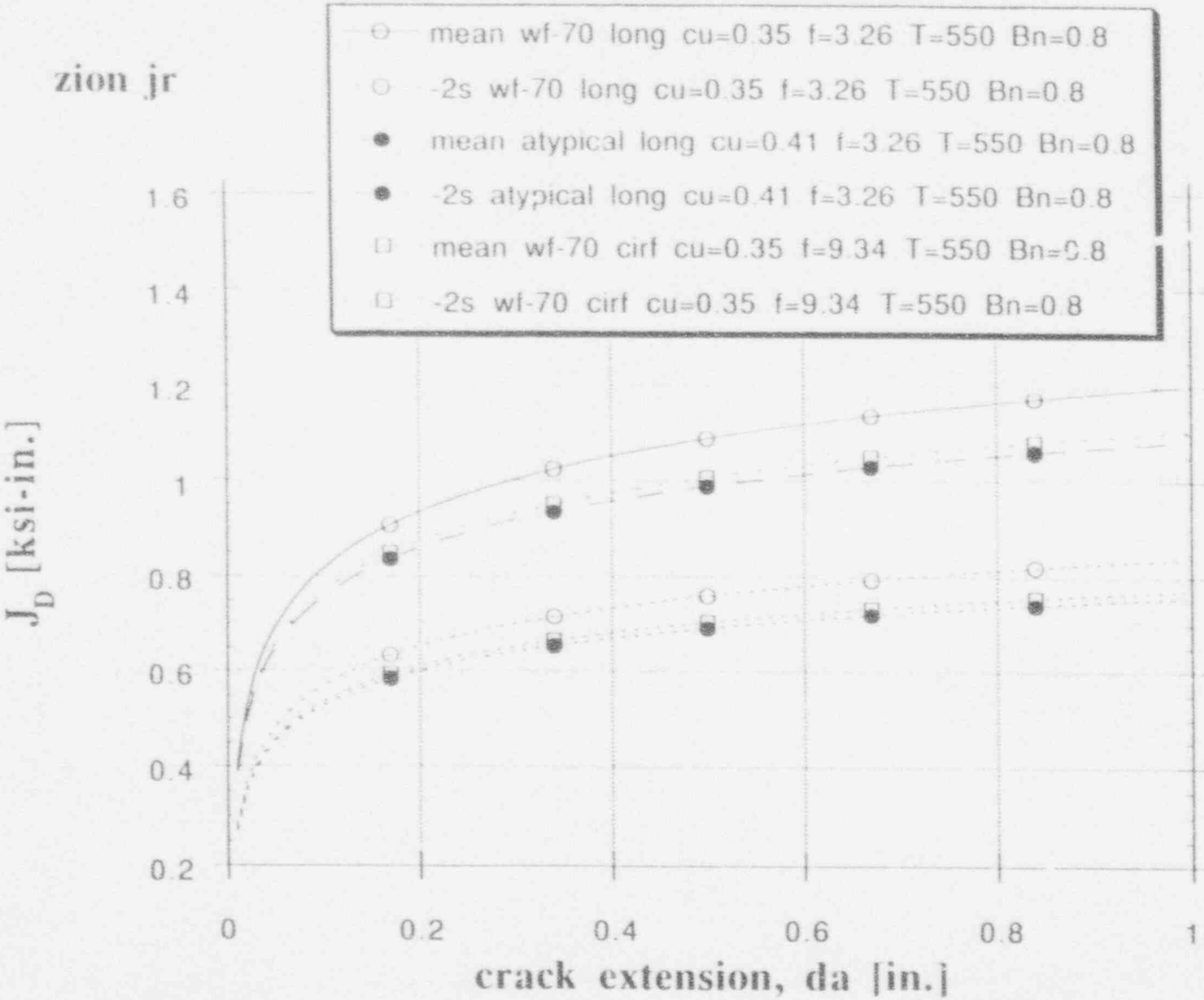
Fig. 11 Comparison of calculated $-2\sigma$ J-R curves, based on Eason-B&W and Eason-NRC correlations, and the $J_{applied}$ curves based on Code Case N-512 analysis procedures. The reference temperature is 530°F with an associated value of the Young's Modulus E = 26,784 ksi.

Fig. 12 Comparison of calculated $-2\sigma$ J-R curves, based on Eason-B&W and Eason-NRC correlations, and the $J_{applied}$ curves based on Code Case N-512 analysis procedures. The reference tempetature is 550°F with an associated value of the Young's Modulus E = 26,680 ksi.

Fig. 13 Comparison of temperature sensitivity between Eason-B&W and Eason-NRC correlations. Comparison is based on toughness values for 0.1 in. crack extension ($J_{0.1}$) and net-section thickness of 0.8 in.

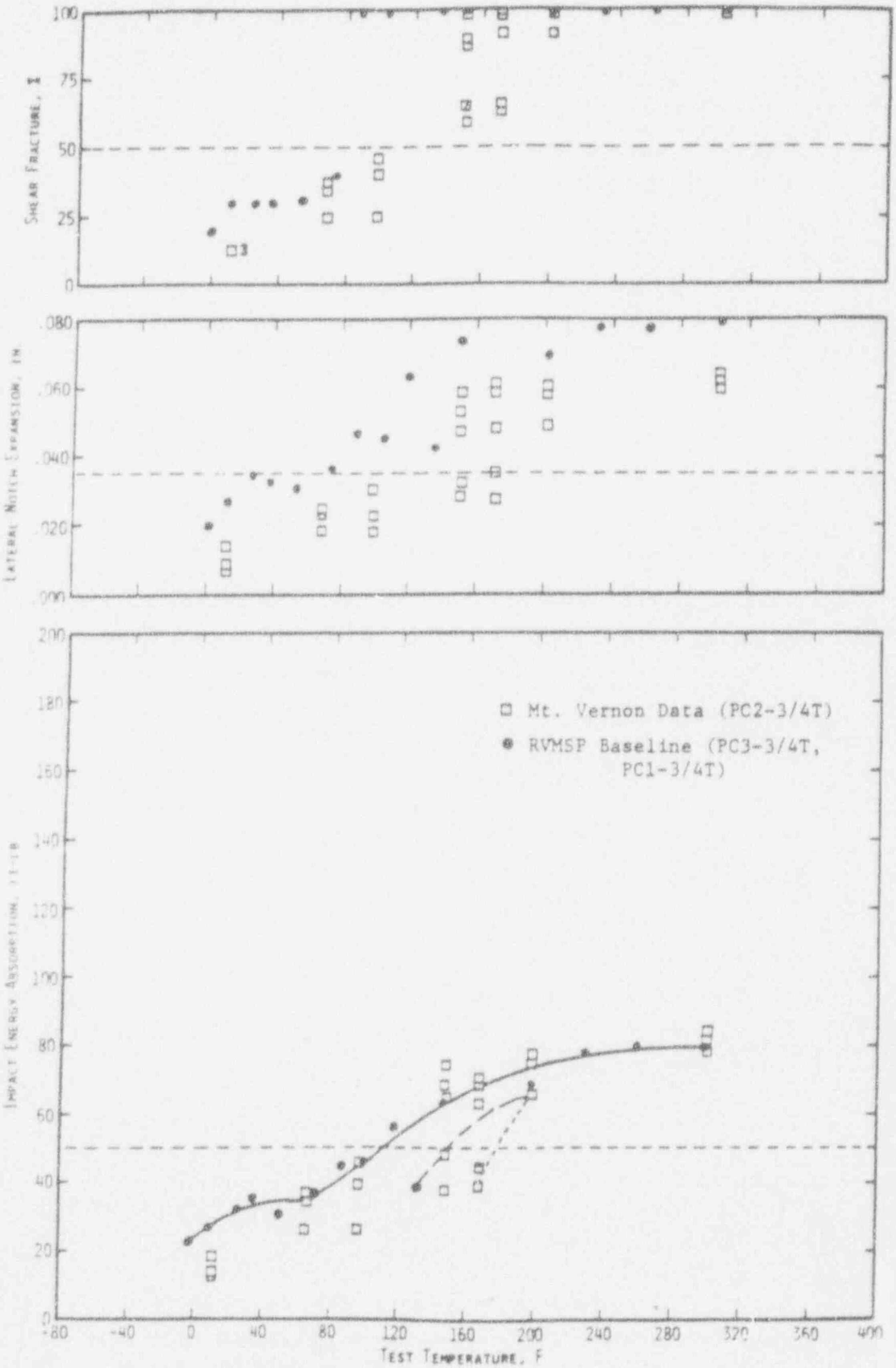
Fig. 14 Comparison of normalized temperature sensitivity between Eason-B&W and Eason-NRC correlations based on a reference temperature of 550°F. Comparison is based on toughness values for 0.1 in. crack extension ($J_{0.1}$ and net-section thickness of 0.8 in.

Fig. 15 Revised comparison of calculated -2σ J-R curves, based on Eason-B&W and Eason-NRC correlations and fluences given in Ref. 45, and the $J_{applied}$ curves based on Code Case N-512 analysis procedures. The reference temperature is 530°F with an associated value of the Young's Modulus E = 26,784 ksi.

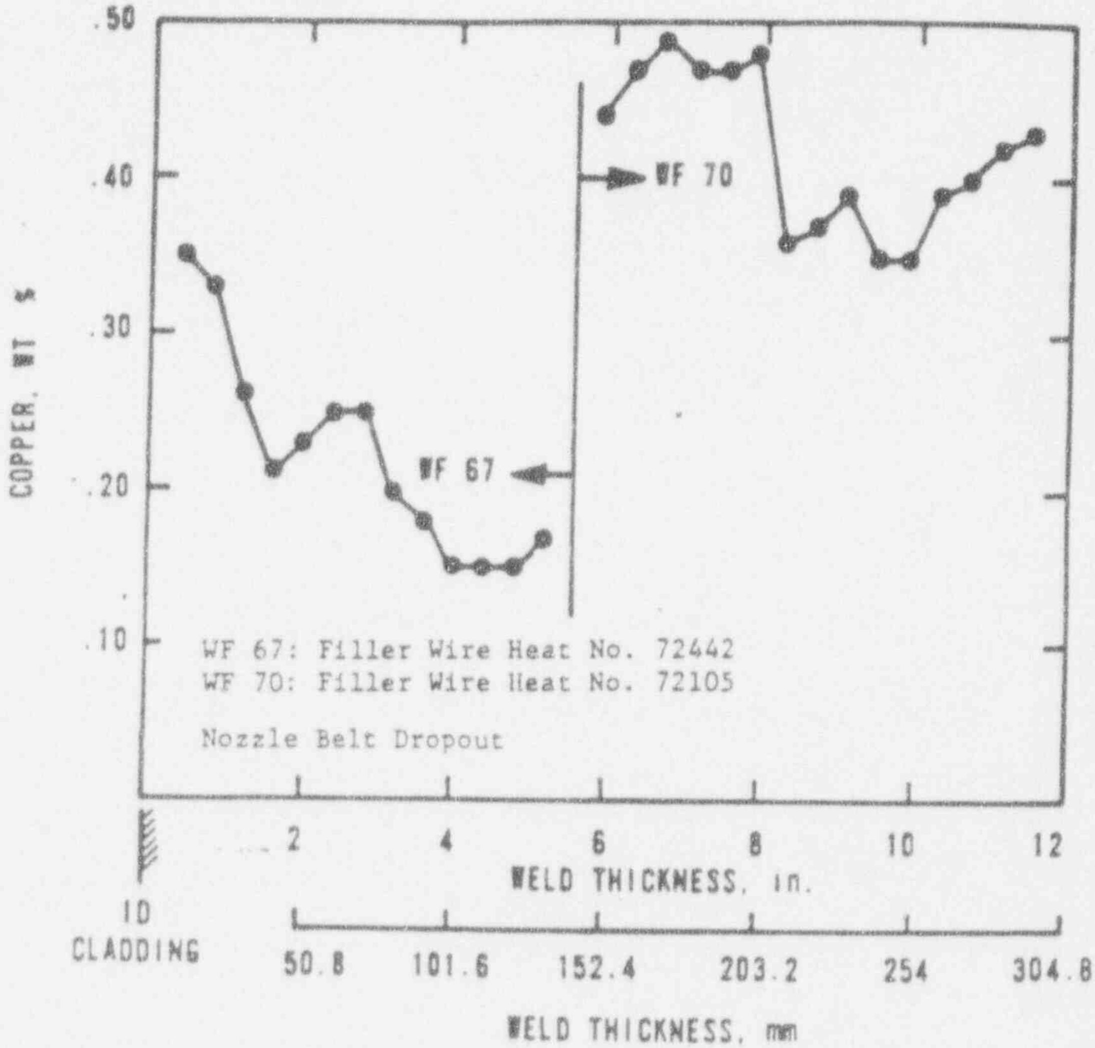


zion jr
mean wf-70 long cu=0.35 f=3.26 T=550 Bn=0.8
-2s wf-70 long cu=0.35 f=3.26 T=550 Bn=0.8
mean atypical long cu=0.41 f=3.26 T=550 Bn=0.8
-2s atypical long cu=0.41 f=3.26 T=550 Bn=0.8
mean wf-70 cirf cu=0.35 f=9.34 T=550 Bn=0.8
-2s wf-70 cirf cu=0.35 f=9.34 T=550 Bn=0.8
$J_D$ [ksi-in.]
crack extension, da [in.]
0.2
0.4
0.6
0.8
1
1.2
1.4
1.6
0
0.2
0.4
0.6
0.8
1


FIGURE 1



Shear Fracture, %
Lateral Notch Expansion, in.
Impact Energy Absorption, ft-lb
Mt. Vernon Data (PC2-3/4T)
RVMSP Baseline (PC3-3/4T, PC1-3/4T)
Test Temperature, F




Note: Two wire/flux combinations.

FIGURE 3

WF-70, COPPER CONTENT

BAW - 2100, 1/93, TABLE 5-5

N = 71

MEAN = 2652/71 = 37.35

STANDARD DEVIATION = 6.42

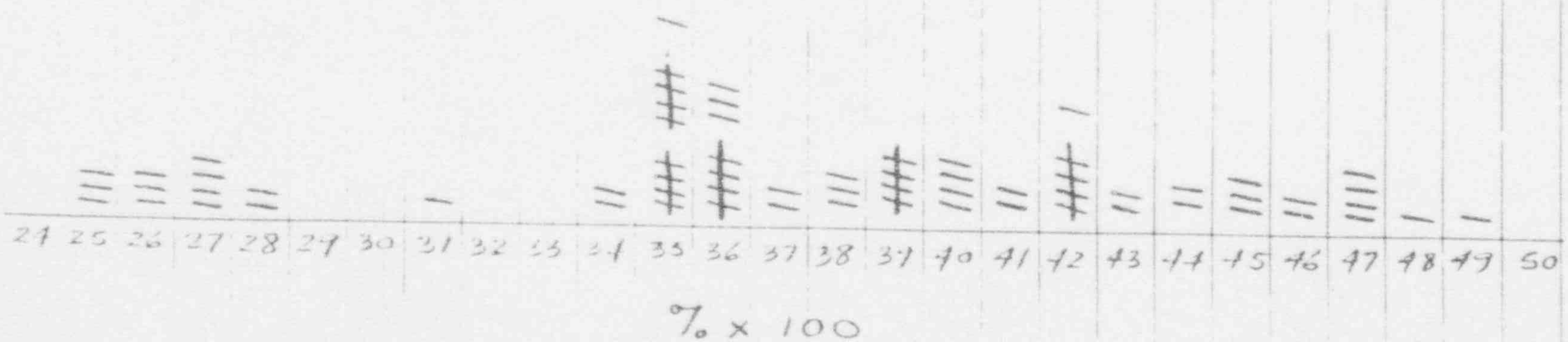
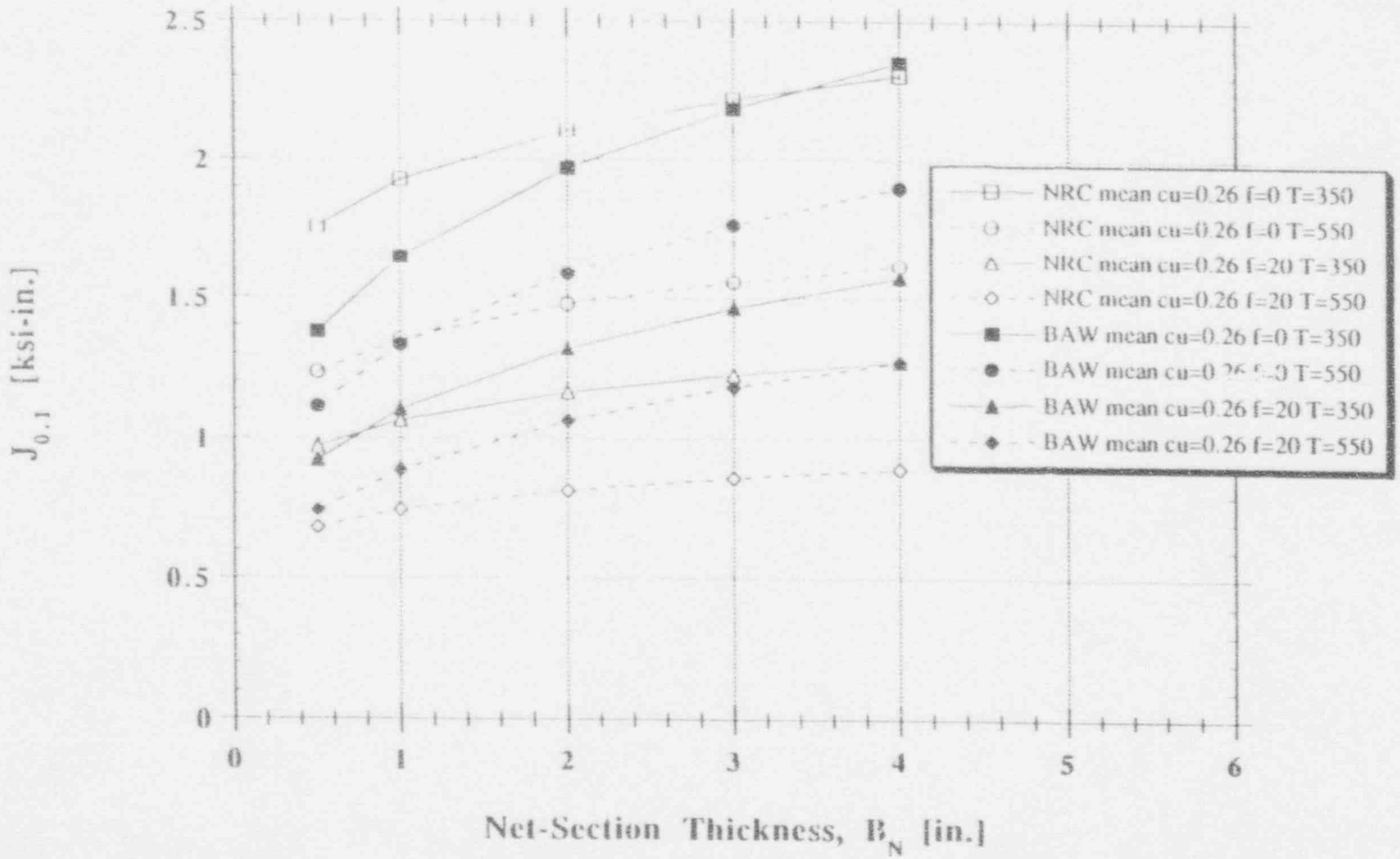
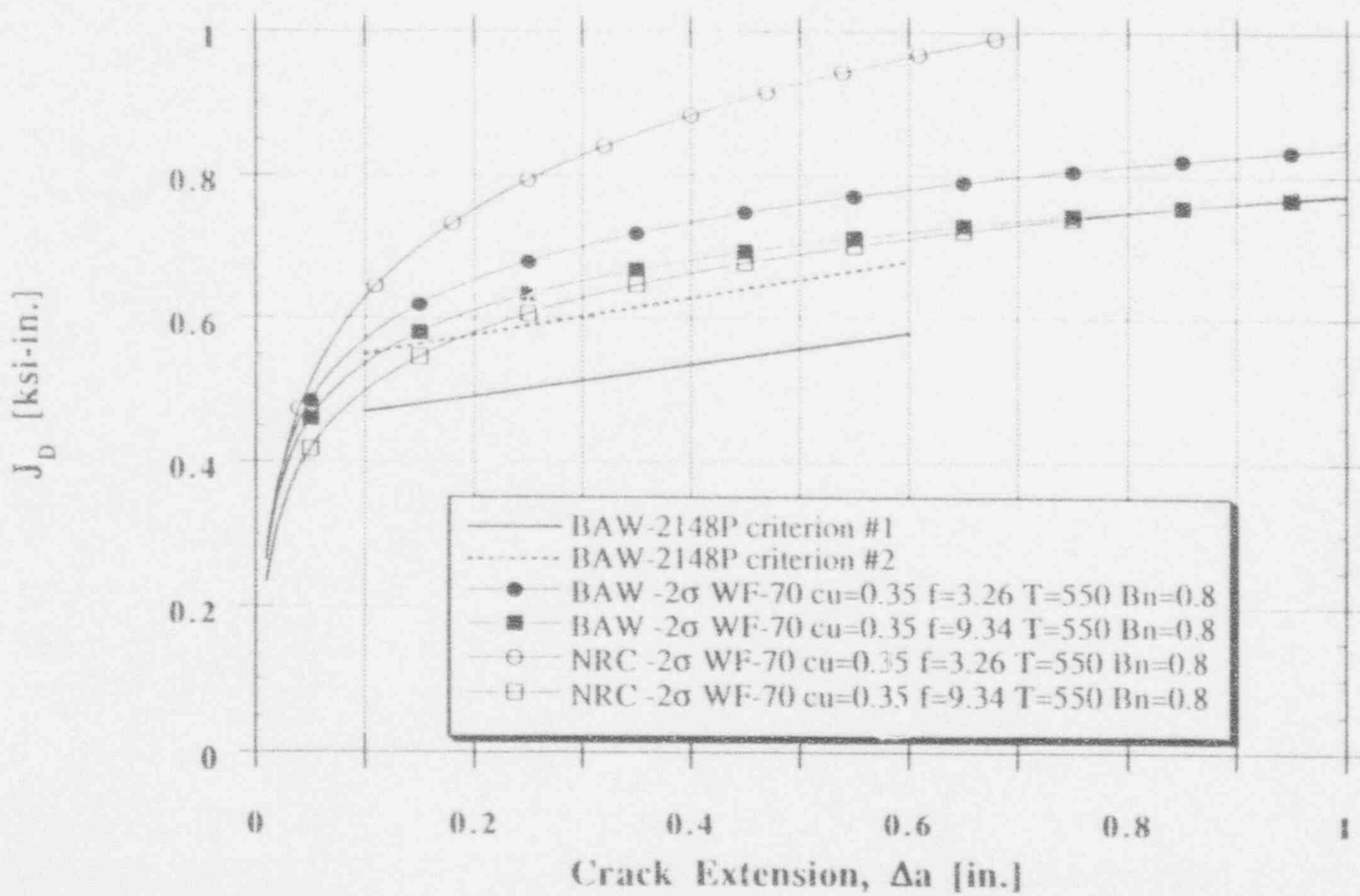



FIGURE 1



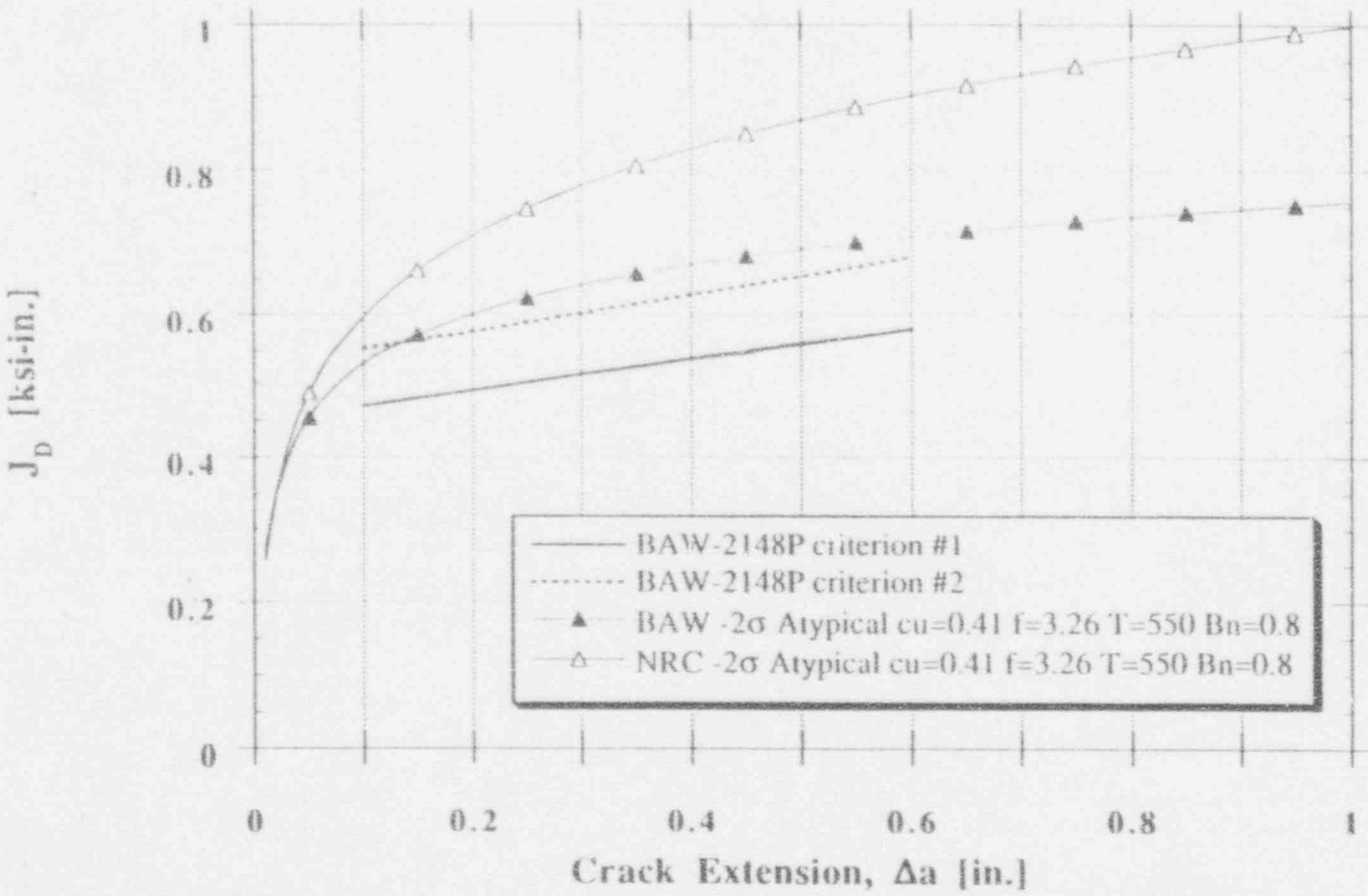
2.5
2
1.5
1
0.5
0
0
1
2
3
4
5
6
$J_{0.1}$ [ksi-in.]
Net-Section Thickness, $B_N$ [in.]
NRC mean cu=0.26 f=0 T=350
NRC mean cu=0.26 f=0 T=550
NRC mean cu=0.26 f=20 T=350
NRC mean cu=0.26 f=20 T=550
BAW mean cu=0.26 f=0 T=350
BAW mean cu=0.26 f=0 T=550
BAW mean cu=0.26 f=20 T=350
BAW mean cu=0.26 f=20 T=550


FIGURE 5



1
0.8
0.6
0.4
0.2
0
$J_D$ [ksi-in.]
0
0.2
0.4
0.6
0.8
1
Crack Extension, Δa [in.]
BAW-2148P criterion #1
BAW-2148P criterion #2
BAW -2σ WF-70 cu=0.35 f=3.26 T=550 Bn=0.8
BAW -2σ WF-70 cu=0.35 f=9.34 T=550 Bn=0.8
NRC -2σ WF-70 cu=0.35 f=3.26 T=550 Bn=0.8
NRC -2σ WF-70 cu=0.35 f=9.34 T=550 Bn=0.8


FIGURE 6



$J_D$ [ksi-in.]
1
0.8
0.6
0.4
0.2
0
0
0.2
0.4
0.6
0.8
1
Crack Extension, Δa [in.]
BAW-2148P criterion #1
BAW-2148P criterion #2
BAW -2σ Atypical cu=0.41 f=3.26 T=550 Bn=0.8
NRC -2σ Atypical cu=0.41 f=3.26 T=550 Bn=0.8


Figure 7

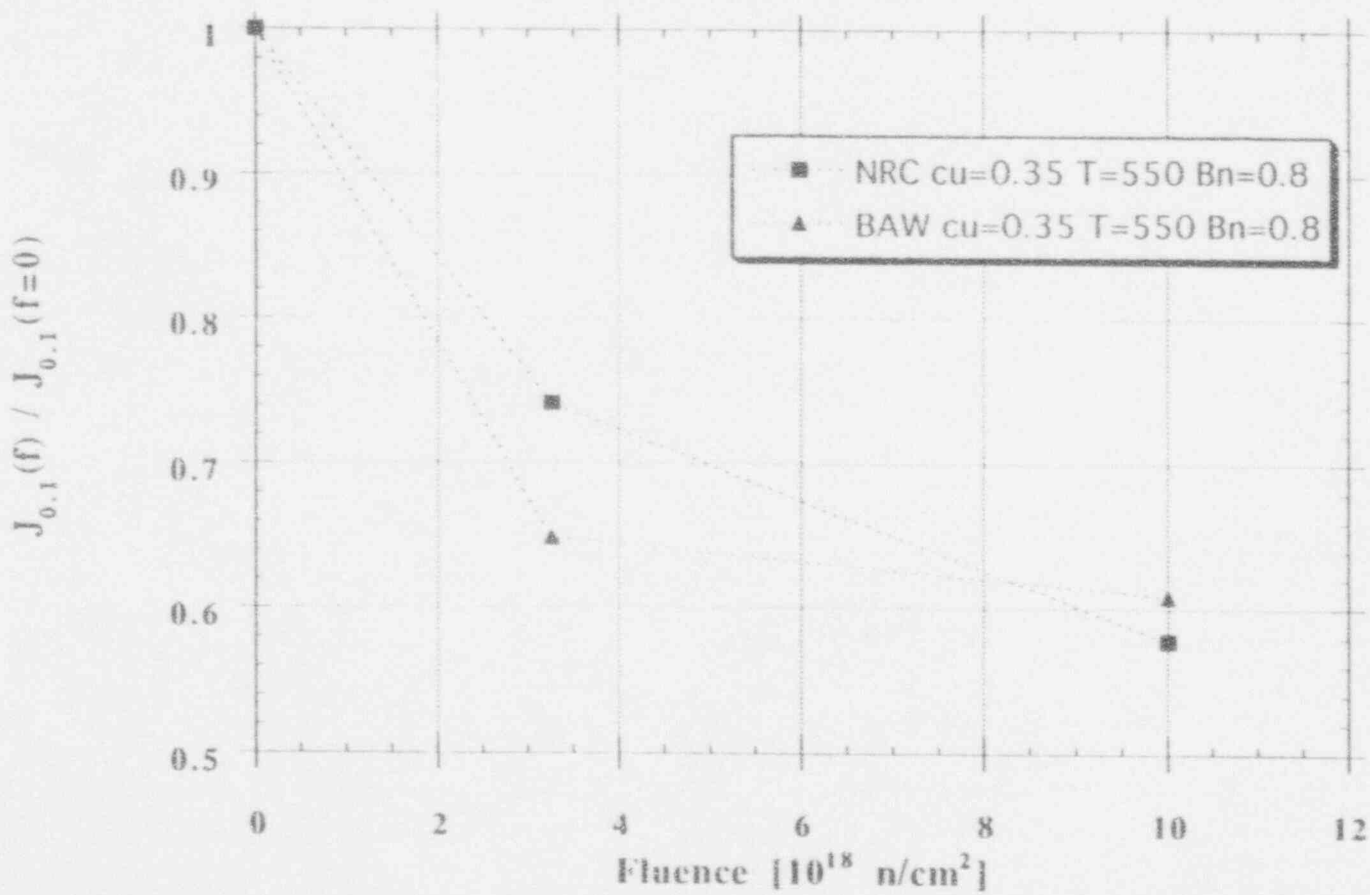
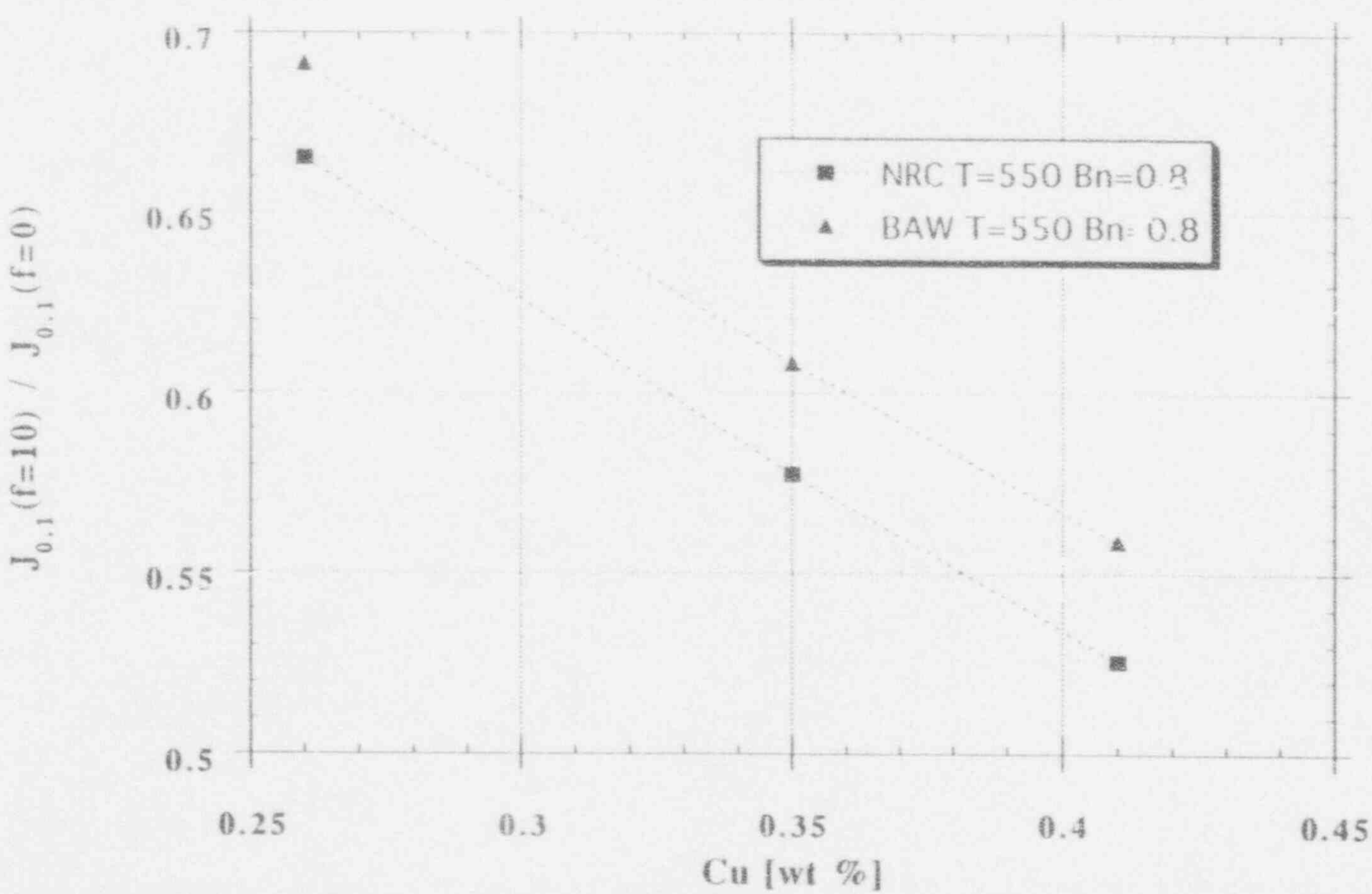

NRC cu=0.35 T=550 Bn=0.8
BAW cu=0.35 T=550 Bn=0.8
$J_{0.1}(f) / J_{0.1}(f=0)$
Fluence [$10^{18}$ n/cm$^2$]
1
0.9
0.8
0.7
0.6
0.5
0
2
4
6
8
10
12

Figure 9



NRC T=550 Bn=0.8
BAW T=550 Bn=0.8
0.7
0.65
0.6
0.55
0.5
$J_{0.1}(f=10) \;/\; J_{0.1}(f=0)$
0.25
0.3
0.35
0.4
0.45
Cu [wt %]


FIGURE 9

## APPENDIX G FLAW

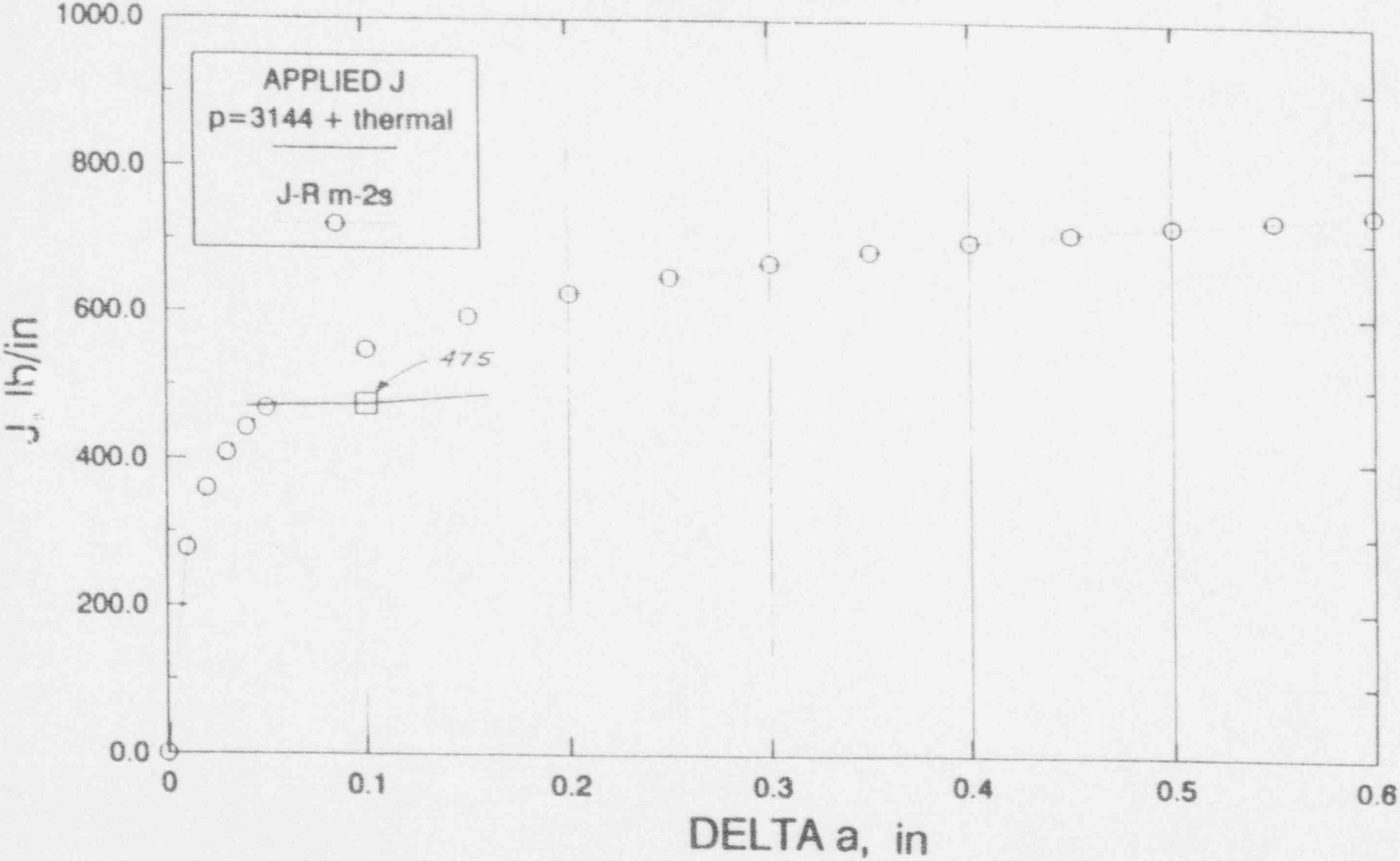
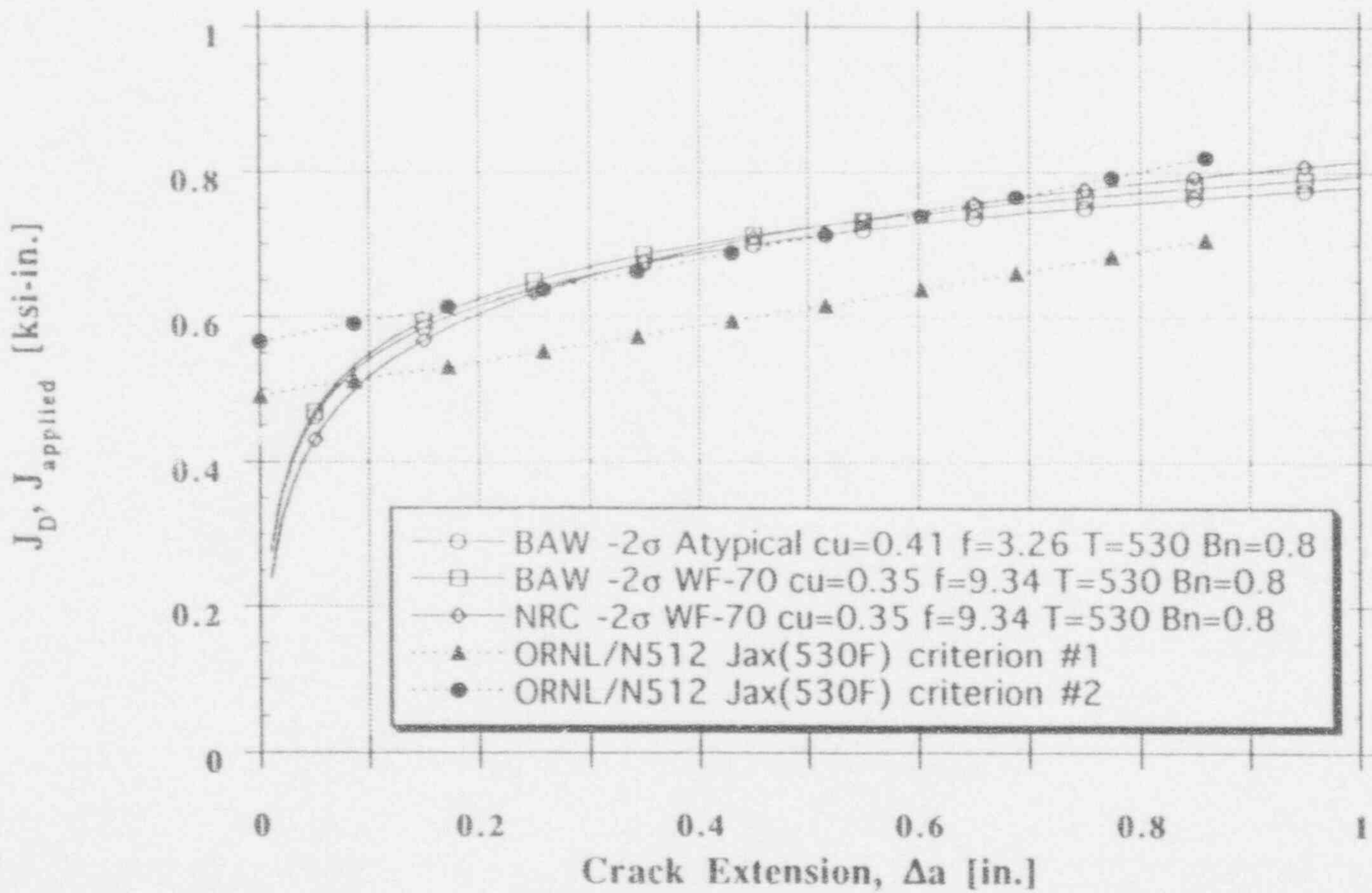
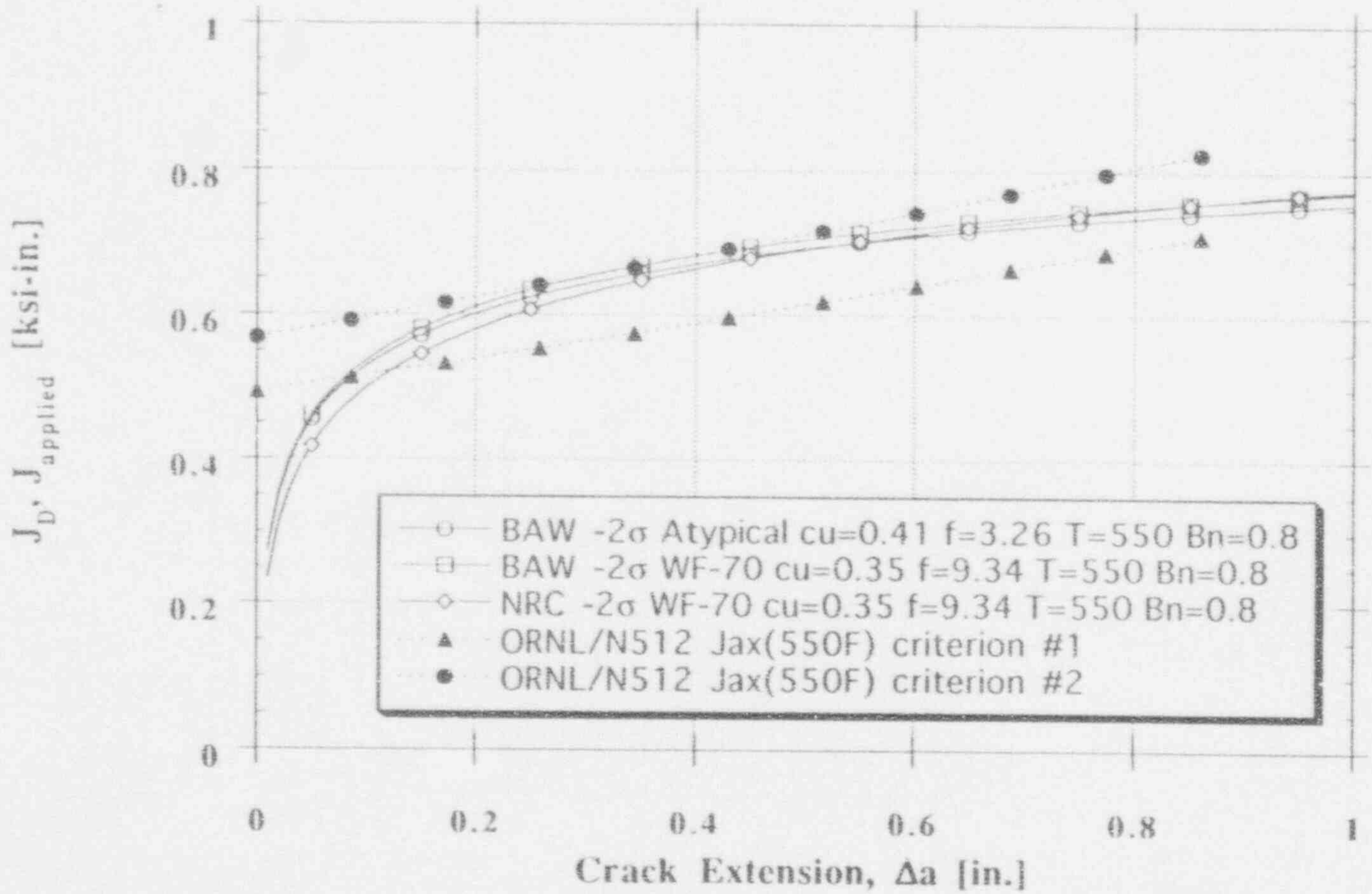



Figure 10



1
0.8
0.6
0.4
0.2
0
$J_D$, $J_{applied}$ [ksi-in.]
0
0.2
0.4
0.6
0.8
1
Crack Extension, Δa [in.]
BAW -2σ Atypical cu=0.41 f=3.26 T=530 Bn=0.8
BAW -2σ WF-70 cu=0.35 f=9.34 T=530 Bn=0.8
NRC -2σ WF-70 cu=0.35 f=9.34 T=530 Bn=0.8
ORNL/N512 Jax(530F) criterion #1
ORNL/N512 Jax(530F) criterion #2


Figure 11



1
0.8
0.6
0.4
0.2
0
0
0.2
0.4
0.6
0.8
1
$J_D$, $J_{applied}$ [ksi-in.]
Crack Extension, Δa [in.]
BAW -2σ Atypical cu=0.41 f=3.26 T=550 Bn=0.8
BAW -2σ WF-70 cu=0.35 f=9.34 T=550 Bn=0.8
NRC -2σ WF-70 cu=0.35 f=9.34 T=550 Bn=0.8
ORNL/N512 Jax(550F) criterion #1
ORNL/N512 Jax(550F) criterion #2


FIGURE 12

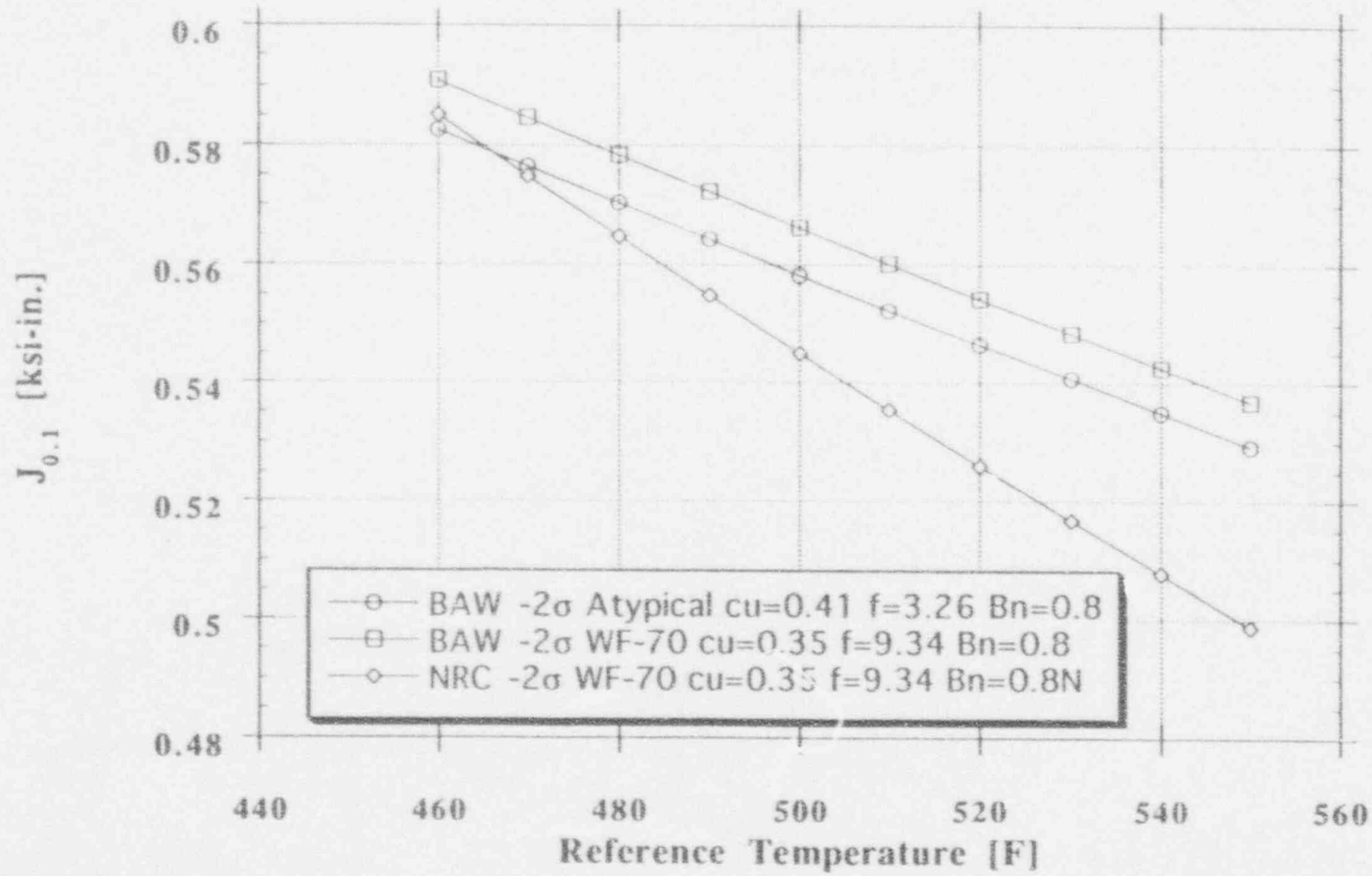

0.6
0.58
0.56
0.54
0.52
0.5
0.48
$J_{0.1}$ [ksi-in.]
440
460
480
500
520
540
560
Reference Temperature [F]
BAW -2σ Atypical cu=0.41 f=3.26 Bn=0.8
BAW -2σ WF-70 cu=0.35 f=9.34 Bn=0.8
NRC -2σ WF-70 cu=0.35 f=9.34 Bn=0.8N

Figure 12

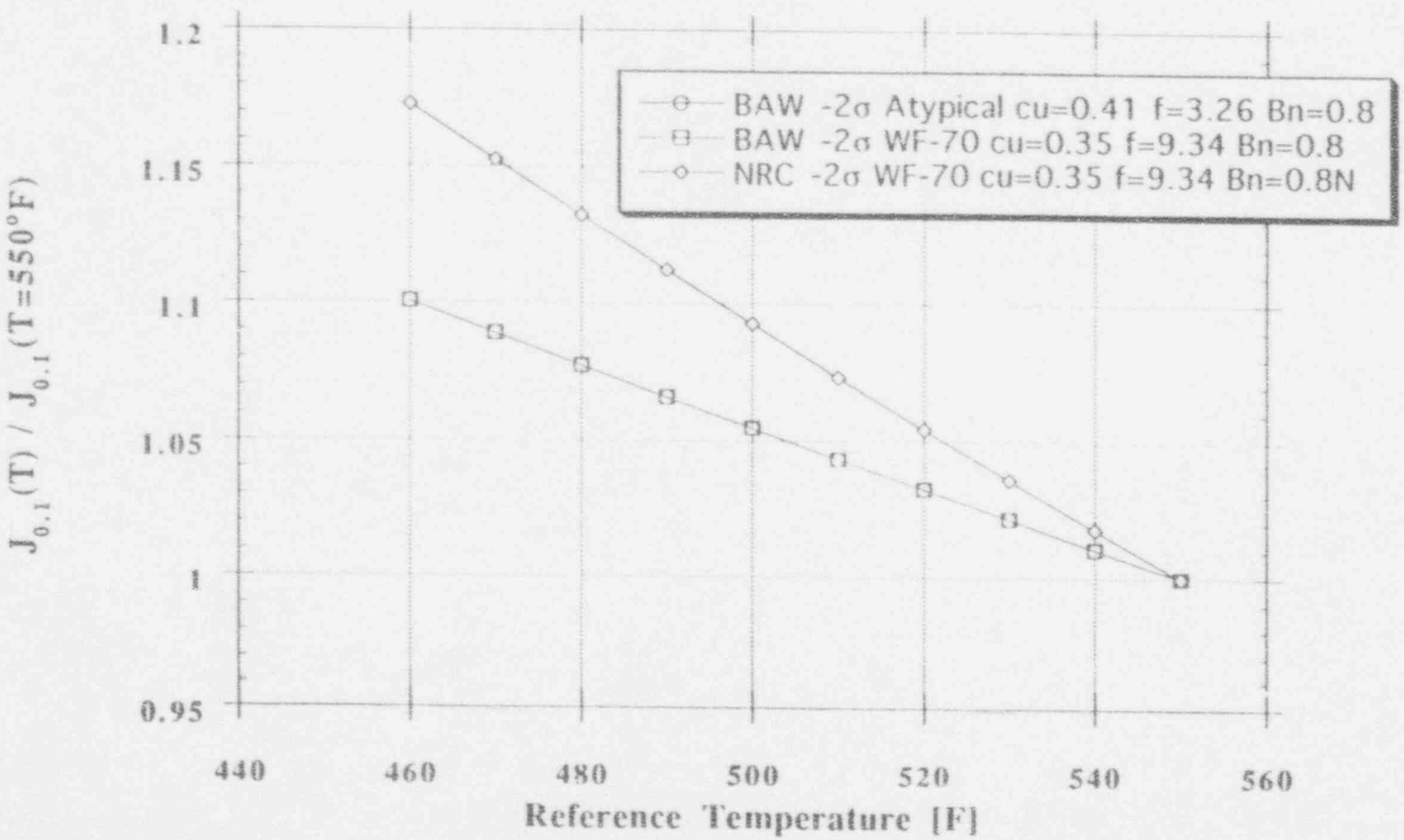
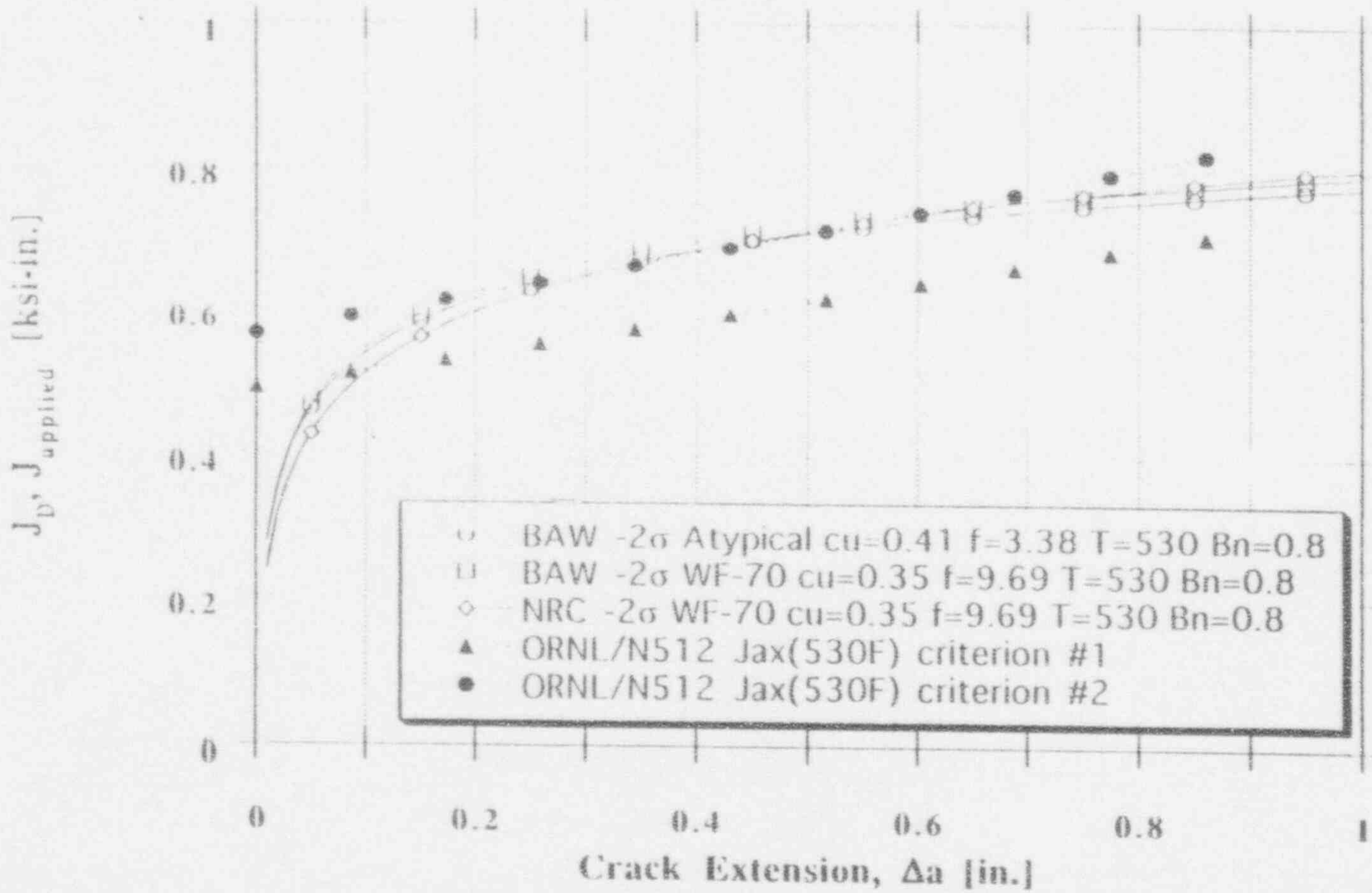

1.2
1.15
1.1
1.05
1
0.95
440
460
480
500
520
540
560
Reference Temperature [F]
J0.1(T) / J0.1(T=550°F)
BAW -2σ Atypical cu=0.41 f=3.26 Bn=0.8
BAW -2σ WF-70 cu=0.35 f=9.34 Bn=0.8
NRC -2σ WF-70 cu=0.35 f=9.34 Bn=0.8N

Figure 14



1
0.8
0.6
0.4
0.2
0
0
0.2
0.4
0.6
0.8
1
$J_D$, $J_{applied}$ [ksi-in.]
Crack Extension, Δa [in.]
BAW -2σ Atypical cu=0.41 f=3.38 T=530 Bn=0.8
BAW -2σ WF-70 cu=0.35 f=9.69 T=530 Bn=0.8
NRC -2σ WF-70 cu=0.35 f=9.69 T=530 Bn=0.8
ORNL/N512 Jax(530F) criterion #1
ORNL/N512 Jax(530F) criterion #2


FIGURE A1