Enclosure 2

TECHNICAL EVALUATION REPORT (TER): REVIEW OF NMPC PROJECT 03-9425 REPORTS, "ELASTIC-PLASTIC FRACTURE MECHANICS ASSESSMENT OF NINE MILE POINT UNIT 1 BELTLINE PLATES FOR SERVICE LEVEL A AND B LOADINGS (MPM-USE-129215) AND SERVICE LEVEL C AND D LOADINGS (MPM-USE-293216)"

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CONTENTS

ABSTRACT

This report contains a review of MPM-USE-129215 and 293216, describing low upper-shelf safety margin analyses of the Nine Mile Point, Unit 1, (NMP-1) reactor pressure vessel, for Levels A, B, C, and D loading conditions. The major aspects of this review concern potentially unique upper-shelf characteristics of A302-B plate, including directional properties, scarce data, reverse size effects, and lack of well established correlations between Charpy impact and J-R data; a detailed review of the NMPC J-R curve estimating procedure; the development of an alternate procedure; and the performance of checking calculations to independently estimate LUS safety margins for the NMP-1 vessel. The checking calculations show that the NMP-1 vessel has adequate margins of safety against ductile tearing in low upper-shelf A302-B plates, at presently projected end-of-life, according to criteria contained in ASME Section XI Code Case N-512. However, the margins calculated by the writers are not as large as those calculated by NMPC.

INTRODUCTION

Background

The purpose of this Technical Evaluation Report (TER) is to provide an engineering review, including technical and regulatory conclusions, concerning the analyses submitted to the Nuclear Regulatory Commission (NRC) by Niagara Mohawk Power Corporation (NMPC) of the safety margins against ductile fracture for the reactor pressure vessel of Nine Mile Point, Unit 1.[1,2] Ref. 1 covers Level A and B loadings and Ref. 2 covers Level C and D loadings. These analyses are made necessary by the requirements[3] 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[4] which was completed in 1982. Subsequently, the NRC requested[5] 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[6] as well as transmitted recommended criteria to the NRC.[7] 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.[8] Ref. 8 prescribes criteria, safety margins and acceptable analysis methods for Levels A and B. The intent of Ref. 1 is to be consistent in all respects with Ref. 8. While Ref. 8 prescribes criteria and safety margins for Levels C and D, it does not provide specific acceptable analysis procedures. Therefore, the fracture mechanics analysis procedures used in Ref. 2 are those believed by NMPC to be adequate and appropriate for the purpose.

A technical and regulatory overview of the low upper-shelf toughness safety margin issue,[9] 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.[3] 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 submissions contained in the reports being reviewed[1,2] respond to paragraph IV.A.1 in that they contain 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. 3. It was not used in Ref. 5 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. 7. 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 judgment 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. 6.

In addition to 10CFR50, Appendix G, the NRC issued Regulatory Guide 1.99, Revision 2, "Radiation Embrittlement of Reactor Vessel Materials,[10]" 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[11] made the use of R. G. 1.99, Rev. 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.

The development of low-upper-shelf code criteria by 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.[12] The draft regulatory guide has been prepared[13] and has been issued for public comment.

In July 1990 the Yankee Atomic Electric Company submitted a pressure vessel evaluation report[14] 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.[15] 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. 11). To determine the current status of reactor pressure vessel integrity data and evaluations, the NRC issued Generic Letter 92–01 (Ref. 15). 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. 15 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. 15) 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. 10), 15 plants currently have USE values less than 50 ft-lbs.[16] 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. 10 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.[16] 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 data) and explanations for apparent inconsistencies between currently reported and previously reported data.[16] At the time Ref. 16 was issued, some utilities had already commenced analyses to determine if their vessels satisfy the criteria given in ASME Section XI Code Case N-512 (Ref. 8). These plants are listed in Table 3. It is the utility submittals for the plants listed in Table 3 that 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 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.[17] Following the September 1992 meeting, the Nuclear Management and Resources Council (NUMARC) began coordinating the industry responses to Generic Letter 92–01. 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 Generic Letter 92–01 submittals by the end of 1993.[16]

Approach to Technical Review

The approach taken to this technical review consisted of several steps, the first of which was a preliminary reading of Refs. 1 and 2, during which a listing was made of missing information, technical and safety related questions and analysis input and results requiring some degree of verification. Requests for Additional Information (RAI's) were then prepared and forwarded to the NRR technical monitor (TM). Following discussion with the TM, modified RAI's were 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 technical and regulatory conclusions stated later in this report.

The main issues identified during and after the preparation of the RAI's for Nine Mile Point Unit 1 were the following:

1. Potentially unique upper shelf characteristics of A302-B plate, including directional properties, scarce data, reverse size effects, and lack of well established correlations between Charpy impact and J-R data;

2. Necessity for carefully evaluating all aspects of NMP's proposed correlation between Charpy impact data and J-R data for A302-B plate, especially methods for estimating ΔJ, the difference between the plateau level of J and J_{Ic}, the neglect of temperature effects on the J-R curve and the partial neglect of random variability of J-R curves;

3. Development of certain elements of the procedures for performing checking calculations, especially an estimate of the variation of stress intensity factor influence coefficients with vessel radius to wall thickness ratio, Ri/t, and the development of an interpolation routine to enable cladding thickness to be specified as a variable;

DISCUSSION OF ISSUES

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 by the ASME Section XI WGFE to the fact that the pressure relief valves and head seal greatly reduce the probability of pressures exceeding certain limits.[18] 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.[18] 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 originally planned 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[19] (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 P_d, the component design pressure, so that for a vessel design pressure of 1250 psi the accumulation pressure cannot exceed 1375 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 flaw has a ratio of crack depth to surface length of 1/6 and is oriented along the weld of concern or has whatever orientation in low upper shelf base metal is most conservative. 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, if an instability occurs, stability must be regained withing $a/t \leq 0.75$ and the remaining ligament must be safe against tensile instability.

Potentially Unique and Uncertain Upper Shelf Characteristics of A302-B Steel Plate

As discussed in Ref. 9, A302-B steel plate has several potential unique characteristics that can create problems with regard to upper shelf safety margin estimates. Its Charpy upper shelf impact energies are directional, with transverse (TL) values being lower than longitudinal (LT) values. J-R data are scarce, and those data that do exist are TL data that exhibit reverse J_D size effects, with the J_D-R curves falling as specimen size increases.[9,20] Furthermore, available data correlations relating Charpy upper-shelf impact energy, plus other toughness related quantities, to parameters of the J-R curve either partially or totally omit A302-B steel plate, making it necessary to improvise estimating relationships on an ad hoc basis.

Present reactor vessel Charpy impact energy requirements are expressed in terms of transverse (TL) properties. However, in the case of older US reactor vessels, TL Charpy data were usually not originally required to meet code design requirements. Therefore, in the absence of TL data, it is frequently necessary to estimate TL values from measured LT values. For this purpose, the NRC prescribes a conservative TL/LT ratio[21] of 0.65. The basis for this ratio is an examination of several sets of TL and LT data for both unirradiated and irradiated A302-B and A533-B steel plate.[22] A summary of the data examined in Ref. 22 is given in Table 4. The weighted average of all the TL/LT ratios listed in Table 4 is 0.743. The minimum and maximum TL/LT ratios listed in Table 4 are 0.56 and 0.97. On the basis of the plant specific unirradiated LT and TL data for plate G-8-3 listed in Table 1–1 of Ref. 1, (see also p. 7 and footnote 1 in Table 1–2 of Ref. 1) the TL/LT ratio for NMP plates was estimated in Ref. 1 as 0.80. The EOL USE TL values for NMP plates G-8-1 and G-307-4 were then estimated in Table 1–2 of Ref. 1 to be both above 50 ft-lbs. However, if a TL/LT ratio of 0.65 had been used, neither EOL USE TL value would have been above 50 ft-lbs. by either the generic or the plant specific procedures in Reg. Guide 1.99, Rev. 2. The NMP estimate of a TL/LT ratio of 0.80 appears to be based on either a single or only a few measurements. In light of the fact that a lesser average value is estimated from the 66 sets of data listed in Ref. 22 and summarized in Table 4, the NMP estimate is not entirely convincing.

The problem of estimating Charpy impact energies when the only available data are for another specimen orientation does not always involve estimating TL values from LT data. Sometimes the problem occurs in reverse. In the case of NMP-1, the only available unirradiated data for the governing plates, G-8-1 and G-307-4, are LT data. If necessary, the determination of a required irradiated LT USE value that would ensure a minimum specified margin for a circumferential flaw would involve calculating a TL value based on a Charpy - J-R curve correlation and then estimating the corresponding LT USE value. For this purpose, the assumption of a TL/LT ratio of 0.65 would be unconservative. Two approaches that would be about equally as conservative as is the 0.65 ratio for estimating TL values from LT data would be either a value of the TL/LT ratio that is the same multiple of the average value, 0.743, as the average value is of 0.65, or a value that is the same percentage above the average as 0.65 is below. The first value of the TL/LT ratio is 0.849. The second is 0.836. Thus, for estimating required LT USE values from calculated TL values a LT/TL ratio near 1.20 would be appropriate. As explained later, this calculation did not turn out to be necessary for NMP-1.

Estimating the J-R Curve

Presently, the only data available upon which to base a J-R curve estimate for A302-B steel are a series of TL measurements made by Hiser and Terrell[20] on specimens ranging in size from 0.5T to 6T. The data for specimens 1T and larger are shown in Fig. 1. Unlike other data for A533-B steel plate and Linde 80 welds, the TL direction J-R curves for A302-B steel exhibit reverse size effects, with the J-R curves falling as specimen size increases. The necessity for conservatism requires that the lowest J-R curve in Fig. 1 be selected as the basis for estimating J-R curves for A302-B steel. The lowest curve in Fig. 1 is from specimen V50–101, a 6T specimen tested at 180°F. Assuming that the data from specimen V50–101 govern the shape of the J-R curve, the development of a J-R curve estimating procedure for A302-B steel requires the formulation of a procedure for adjusting the curve to account for variations in Charpy upper-shelf impact energy and temperature, plus accounting for random variability. It is reasonable to assume that specimen orientation does not affect the correlation between J-R curve parameters and Charpy upper-shelf impact energy, when both quantities pertain to the same crack plane orientation. On this basis, specimen orientation effects can be taken into account by selecting a TL/LT ratio for the Charpy upper-shelf impact energies.

The first empirical correlation for estimating the parameters of a power law J-R curve was developed by Merkle[23] and Dougan.[24] The data base for this correlation included some, but not all, of the A302-B data reported by Hiser and Terrell.[20] This correlation involved the tensile flow stress as well as the Charpy upper-shelf impact energy. Shortly afterwards, Hiser developed a revised correlation,[25] not involving the flow stress, which NMP, in their Level A and B RAI response, has used to develop an estimate of ΔJ, the difference between $J_{0.1}$ and J_{Ic}. This estimate is partially described on pp. 6–7 and in Table 2.1–1 of the NMP Level A and B RAI response. The calculation of $J_{0.1}$ is straightforward, but the calculation of J_{Ic}, which presumably follows ASTM E813, is not described. Table 2.1–1 of the Level A and B RAI response shows that calculated values of ΔJ increase with increasing USE, becoming greater than 175 in.-lbs./in.^{2}, the value measured with specimen V50–101, for a value of USE somewhere between 30 and 40 ft/lbs. NMP developed a separate correlation for estimating the -2σ value of J_{Ic} as a function of USE, as described on p. 17, in Fig. 4–12 and Table 4–5 of Ref. 1 and in Table 3–1 of the RAI response for Ref. 2. Recognizing the flatness of the specimen V50–101, J-R curve in Fig. 1 for values of $\Delta a > 0.10$ in., the RAI response for Ref. 1 estimated the plateau level of J as $J_{Ic} + \Delta J$. The value of ΔJ was truncated at 175 in lbs/in.^{2}, the value measured for specimen V50–101. Although the value of J thus estimated was treated as a - 2σ value, only the J_{Ic} portion actually had that statistical significance, because the calculated ΔJ values less than 175 in lbs/in.^{2} were based on a mean correlation, and the value 175 in lb/in.^{2} was obtained from a single specimen. No variation of the plateau level of J with temperature was considered, relying incorrectly on the sometimes constant value of the Charpy impact energy in the upper-shelf temperature range.

Another procedure for estimating the J-R curve for relatively high sulfur ($S > 0.018\%$), low upper-shelf steels, including A302-B steel, has been developed by the NRC and is described in Ref. 26. This procedure also involves adjusting the measured J-R curve from specimen V50–101 to take into account the effect of variations in Charpy upper-shelf impact energy and temperature, and uses an uncertainty factor to represent the effects of random variability. The approach is based on the observed separability of variables in the Eason correlating equations[27] for estimating the J-R curve for other reactor primary system base metals and welds. In fact, it uses a temperature factor obtained directly from the Eason correlations,[27] although the Charpy impact energy, CVN, factor is from another source. The J-R curve from specimen V50–101 is represented in Ref. 26 by a power law fitted by Hiser[28] to the measured J-R data for $\Delta a \leq 0.10$ in. The J-R curve estimating equation proposed in Ref. 26 has the form

$$J = (SF)C_1 (\Delta a)^{C_2} [f(CVN)][g(T, \Delta a)], \tag{1}$$

where SF is a statistical uncertainty factor, $C_1 (\Delta a)^{C_2}$ is Hiser's power law fit to specimen V50–101 data,[28] f(CVN) is a mean correlation between $J_{0.1}$ and CVN based on Hiser's analysis of available A302-B data,[29] normalized to CVN = 50 ft-lbs., and g(T, Δa) is a temperature variation factor obtained from Eason's correlations.[27] The equations for the factors in Eq. (1) are[26]

$$C_1 (\Delta a)^{C_2} = 946.82 (\Delta a)^{0.1334}, \tag{2}$$

$$f(CVN) = \frac{11.75CVN + 108}{695.5}, \tag{3}$$

and

$$g(T, \Delta a) = \exp.\left[-0.00277(T - 180)\left\{1 + 0.116\ln(\Delta a) - 0.0092(\Delta a)^{-0.409}\right\}\right]. \tag{4}$$

For Δa = 0.10 in., the increase of g(T, Δa) from 550°F to 392°F is 36% and to 180°F it is 52%.

As shown in Fig. 2, the fit of Eq. (2) to the V50–101 data for Δa ≤ 0.10 in. is quite good. However, as shown in Fig. 3, the sudden decrease in slope of the measured J-R curve at Δa = 0.10 in. causes the power law fit to become significantly unconservative for greater values of Δa.

Further examination of Eq. (1) clarifies its basic features. Fig. 4 shows that the composite effects of Eqs. (2) and (4) are to produce an essentially flat J-R curve near the NMP-1 operating temperature of 547°F. Fig. 5 shows the same curves normalized by $J_{0.1}$. Again, the flatness of the J-R curve near the NMP operating temperature is illustrated. The nature of Eq. (4) by itself is revealed by Fig. 6 which shows that above 180°F, the temperature adjustment increases with increasing Δa, and below 180°F, the reverse is true. Eq. (3) is plotted in Fig. 7, where CVN_R is the reference mean value of CVN corresponding to the crack plane orientation for a particular J-R curve. Defining an uncertainty factor, UF, as a scaling factor to be applied to the mean J-R curve to obtain a J-R curve of particular statistical significance and using UF = 1, the mean value of $J_{0.1}$ vs CVN_R is shown in Fig. 8. Fig. 9 compares the same mean $J_{0.1}$ curve from Fig. 8 with the NMP former (ΔJ = 175 in lbs./in.^{2}) curve based on Ref. 1 and the present (variable ΔJ) curve based on the NMP Level A and B RAI response. Clearly the former NMP curve is not A -2σ curve because of its reliance on a single specimen value of ΔJ, and the present NMP curve would be noticeably above a -2σ modified 302 curve, for CVN > 30 ft./lbs., based on a value of SF near 0.7. It thus appears that, apart from Eq. (2) overestimating the data from specimen V50–101 for Δa > 0.10 in., the J-R curve estimating procedure for A302-B steel suggested in Ref. 26 is supperior to that developed in Ref. 1 and its associated RAI response because the former is free of hidden bias, more physically complete, and conservative.

In order to remedy the overestimation tendency of Eq. (2) for Δa > 0.10 in., Eq. (1) has been modified by the writers for the purpose of performing checking calculations concerning Nine Mile Point Unit 1. Eq. (1) has been retained, with only one modification, for Δa ≤ 0.10 in., but J has been assumed constant for $\Delta a > 0.10$ in. The one modification of Eq. (1) for $\Delta a \leq 0.10$ in. pertains to orientation effects. Recognizing that Eq. (1) holds for identical crack plane orientations of Charpy and J-R specimens, but that calculations may be required when the available value of CVN does not correspond to the crack plane orientation, CVN is replaced in Eq. (3) with CVN · OF, where OF is the orientation factor relating the available value of CVN to the value corresponding to the crack plane orientation. Thus

$$J = (UF)\, C_1(\Delta a)^{C_2}[f(CVN \cdot OF)][g(T, \Delta a)], \quad \Delta a \leq 0.10 \text{in.}, \tag{5}$$

and

$$J = J(\Delta a = 0.10 \text{in.}), \; \Delta a > 0.10 \text{in.}, \tag{6}$$

where C_1 and C_2 are the same as in Eq. (2), f is the same function given by Eq. (3), and g is identical to the expression in Eq. (4). The value of OF is 1.0 when available CVN data correspond to the chosen flaw orientation.

DEVELOPMENT OF ANALYTICAL METHODS

The choice of a strategy for checking the NMP-1 calculations is governed by the need to separate matters concerning the uncertainty of inputs from matters concerning the accuracy and completeness of analytical methods, so that each subject can be considered carefully without interference from the other. The major uncertainties pertain to the best numerical values to use for the factors UF, the statistical uncertainty factor relating -2σ to mean values of the ordinates to the J-R curve, and OF, the orientation factor relating TL to LT values of Charpy USE. Therefore, flaw evaluation calculations will be performed in terms of the reference value CVN_R, as explained below, and the actual directional values of CVN will be calculated separately, over a range of UF and OF values. Returning to Eqs. (2) through (5), it can be seen that the only factors in Eq. (5) that involve UF and OF are UF itself and f(CVN·OF). Therefore, for a given J-R curve, it is possible to write

$$UF \cdot f(CVN \cdot OF) = f(CVN_R), \tag{7}$$

where CVN_R is the mean value of CVN corresponding to the crack plane orientation. The reference value, CVN_R, in conjunction with temperature, corresponds uniquely to the ordinates to a particular J-R curve, independent of UF and OF. Thus, flaw evaluation calculations can be performed in terms of CVN_R, and the directional value of CVN with a particular statistical significance can be determined separately by solving Eq. (7) for CVN. Combing Eqs. (3) and (7) gives

$$UF\left(\frac{11.75\, CVN \cdot OF + 108}{695.5}\right) = \frac{11.75\, CVN_R + 108}{695.5}, \tag{8}$$

and solving Eq. (8) for CVN gives

$$CVN = \frac{108\left(\frac{1}{UF}-1\right)}{11.75\,(OF)} + \frac{CVN_R}{(OF)(UF)}. \quad (9)$$

Fig. 10 shows the linear relationships between CVN and CVN_R for three different values of UF, with OF = 1. The calculated ratios of CVN_{TL}/CVN_R for various combinations of UF and OF are shown in Fig. 11. Although the relationship between CVN and CVN_R remains linear, according to Eq. (9), Fig. 11 emphasizes that the ratio CVN_{TL}/CVN_R increases as OF decreases, and as UF and CVN_R decrease for UF < 1.

CHECKING CALCULATIONS FOR LEVELS A AND B

The NMP-1 Level A and B results obtained by NMPC, stated in terms of the required minimum longitudinal (LT) values of Charpy USE at end-of-life, are given in Tables 2.1–2 and 2.3–1 in the NMPC RAI response dated September 8, 1993. These tables are reproduced here as Tables 5 and 6. Note that NMP did not perform calculations for the combined effects of variable ΔJ and clad stresses. Checking calculations were performed by the writers, using the Code Case N-512 equations and including the value of $K_{Ires} = 6.6$ ksi$\sqrt{\text{in.}}$ at t/4 estimated by NMP in their RAI response. Input values were as given in Table 7. The applied values of J for both Level A and B criteria, plotted versus additional crack depth relative to t/4, are shown in Fig. 12 for both axial and circumferential flaws. The comparison between J_{ax} for Levels A & B, criterion #1, at Δa = 0.10 in. in Fig. 12 with the corresponding NMP value in Fig. 5–1 of Ref. 1 is good, taking into account that the NMP value does not include the effects of K_{Ires}. Fig. 13 provides a graphical representation of the conversions from Δa, relative to a_o, to the applied value of J and then to CVN_R, for Levels A and B criterion #1, based on Fig. 12 and Eq. (5). Fig. 13 is read by starting at the Δa axis (point 1), progressing to the appropriate applied J curve (point 2), then at the same value of $J_{0.1}$ to the CVN_R vs $J_{0.1}$ curve (point 3) and then to the CVN_R axis (point 4). The value of CVN_R for an axial flaw corresponding to criterion #1 is 33 ft-lbs. Fig. 14 shows the J-R curves that have a point of tangency with the applied J curves shown in Fig. 12. Because tearing instability occurs at the knee of the J-R curve in these cases, the required value of CVN_R for an axial flaw and criterion #2 is slightly higher then for criterion #1, 36 ft-lbs. The governing CVN_R values appear to agree with the NMP-1 LT values of CVN, including the effects of variable ΔJ but not K_{Ires}, as shown in Tables 5 and 6. However, the effects of random variability, expressed in terms of UF, have not yet been taken into account in the checking calculations.

There are no statistical estimates of UF for A302-B steel plate, but statistically calculated values of UF do exist for other primary system base metals and welds. Two such values calculated by Eason et al. for the -2σ level are UF = 0.634 for a RPV Combined (base metals and welds) Preirradiation Charpy -J_d correlation and UF = 0.749 for a Base Metals (postirradiation) Charpy - J_D correlation. Welds are likely to be more variable then base metals, and it seems reasonable to expect that postirradiation Charpy values would lead to a better correlation with irradiated J-R curves than preirradiation Charpy values. Therefore, a value of UF = 0.7, close to the average of the above two numbers, seems a reasonable estimate of UF for A302-B steel. Using Eq. (9), the LT value of CVN corresponding to CVN_R = 36 ft-lbs., OF = 1 and UF = 0.7 is 55 ft-lbs., which agrees with Fig. 10. Referring to Table 1–1 of Ref. 1, the lowest estimated EOL value of USE in the LT direction for a NMP-1 plate is calculated to be 61.6 ft-lbs. for plate G-307-4. Since this value exceeds 55 ft-lbs., the NMP-1 plates appear to satisfy Level A and B criteria, but not by the margins indicated by Tables 5 and 6.

CHECKING CALCULATIONS FOR LEVELS C AND D

Checking calculations for Levels C and D have been performed by the writers primarily with the FAVOR code[30] and secondarily with the methods described in Ref. 13. Estimates of fluence and RT_{NDT} were based on Ref. 1 and Reg. Guide 1.99, Rev. 2.[10] Estimates of K_{Ires} were based on the NMP value of 6.6 ksi$\sqrt{in.}$ at t/4 and the assumption that K_{Ires} is proportional to $\sqrt{a}$. Vessel dimensions used were those listed in Table 8, based on Ref. 2 with one exception explained by Note 1 in Table 7. Thermal properties were taken from Table 3 of Ref. 17. The modulus of elasticity of cladding and base metal were assumed identical and equal to 27,252 ksi, the value for base metal at 440°F based on ASME code data. Poisson's ratio was assumed equal to 0.33. Based on the RAI response for Ref. 2, specific heat was taken as 0.13 BTU/LB·°F and the thermal expansion coefficients for base metal and cladding were taken as 8.11×10^{-6}°F^{-1} and 9.51×10^{-6}°F^{-1} respectively. The heat transfer coefficient was taken to have the values given in the RAI response for Ref. 2.

The stress intensity factor influence coefficients used with FAVOR to calculate K_I due to pressure and thermal loading for a finite length surface flaw with a depth to length ratio of 1/6 were obtained from Ref. 31. These coefficients were calculated for $R_i/t = 10$ and are believed to be accurate within a few percent for the NMP value of $R_i/t = 14.6$. The influence coefficients for cladding were calculated for one particular value of cladding thickness.[31] Therefore, a power law interpolating expression was introduced into the code to vary the coefficients from zero to the base metal values as the ratio t_{cl}/a varies from zero to one. Trial calculations showed that plastic zone size adjustments are small enough to neglect.

Noting from other Level C and D analyses that the maximum value of K_I tends to be monotonically increasing, within the range of flaw sizes specified by Code Case N-512, calculations were performed for $a_o = t_b/10 + t_{cl} = 0.868$ in. It was noted that Ref. 2 used a maximum flaw size of $(t_b + t_{cl})/10 \sim 0.75$ in., which, due to a misinterpretation of Code Case N-512, is too small.

Checking calculations were performed for two transients, the Level C 250°F/7.5 min. blowdown and the Level D steam line break. Fig. 15 shows the time variation of K_I for the Level C transient, for a crack depth of $a_o + 0.10$ in. = 0.968 in. The maximum value of K_I occurs at a transient time of about 10 minutes, close in time and magnitude to the values calculated in Ref. 2. At a transient time of 10 minutes, the through-wall temperature and stress distributions are as shown in Figs. 16 and 17. The comparison with Ref. 2 is good, although the cladding stresses shown in Fig. 17 are higher than those shown in Fig. 4–6 of Ref. 2. Fig. 18 shows the comparison between the applied value of K_I and $K_{J,0.1}$ for the Level C blowdown transient. Considering the effect of temperature on $K_{J,0.1}$, the limiting value of CVN_R for an axial flaw is 13 ft-lbs. Calculations were not performed for a circumferential flaw, relying on Fig. 4–6 of Ref. 2 which shows that the axial stresses are less than the circumferential stresses. Using Eq. (9), OF = 1 and UF = 0.7, the calculated EOL value of CVN for the LT direction is 22.5 ft-lbs., considerably greater than the value of 10 ft-lbs. given on p. 43 and in Table 5–3 of Ref. 2. Fig. 19 shows values of K_I calculated according to Ref. 13, with pressure set equal to the design pressure, P_d, which is 1250 psi. The Level C and D values of K_I bracket the maximum value of K_I calculated by FAVOR. If the toughness at operating temperature must be used to satisfy Code Case N-512 criteria, CVN_R = 25 ft-lbs. and from Eq. (9) the corresponding LT value of CVN is 39.7 ft-lbs. K-R tangency plots for criterion #2 are shown in Fig. 20. Because Level C loads and toughness remain unchanged from crition #1 to Criterion #2 and the knee of the J-R curve at which tearing instability occurs is assumed to be located at $\Delta a = 0.10$ in., satisfying one crition implies satisfying the other.

Calculations for the Level D steam line break transient followed the same sequence as those for the Level C transient, as shown by Figs. 21 thru 26. The difference between the two transients is made evident by comparing Figs. 15 and 21. The Level D steam line break transient produces a higher maximum value of K_I and the maximum occurs sooner in the transient, at a transient time of about five minutes instead of ten. This is close enough to the value of four minutes reported in Ref. 2. The crack depth used for analysis was 0.868 in. The maximum value of K_I also agrees well with Ref. 2. From Fig. 22, the crack tip temperature at the time of the maximum value of K_I is almost the same as for the Level C transient. The stress distribution at the same time shown in Fig 23 also agrees well with Ref. 2, but again shows higher cladding stresses. The variation of applied K_I with crack tip temperature shown in Fig. 24 displays a sharp drop after the peak with a slight temporary increase in crack tip temperature. This phenomenon is due to the occurrence of a minimum in the heat transfer coefficient and the resulting redistribution of heat within the vessel wall. The temperature profiles in Ref. 2 show the same behavior. From Fig. 24, considering the variation of tearing resistance with temperature, the required value of CVN_R for an axial flaw is 21 ft-lbs., higher than the required value of 13 ft-lbs. for the Level C transient. From Eq. (9), using OF = 1 and UF = 0.7, the calculated EOL value of CVN for the LT direction is 33.9 ft-lbs., considerably greater than the value of 20 ft-lbs. given on p. 43 and in Table 5–4 of Ref. 2. Fig. 25 shows the calculated K_I values based on Ref. 13 compared to those calculated by FAVOR. In this case, the calculations based on Ref. 13 are unconservative, probably because, as indicated in Table 3–2 of Ref. 2, the thermal transient is more severe than assumed in Ref. 13. The tearing instability diagrams based on the maximum value of K_I calculated by FAVOR, with and without considering the effect of temperature on the J-R curve, are shown in Fig. 26. Using a J-R curve corresponding to the operating temperature, 528°F, as prescribed for a steady state thermal analysis in Ref. 13, the required value of CVN_R is 36 ft-lbs., the same value obtained for Levels A and B, criterion #2. Thus the corresponding LT value of CVN is 55 ft-lbs., still below the lowest estimated EOL value for plate G-307-4.

DISCUSSION

The calculated governing value of CVN_R obtained herein for Levels A and B, 36 ft-lbs., is identical to the value for a BWR vessel given in Table 12 of Ref. 17 for low toughness plate. However, Ref. 17 used a value of the safety factor, SF, identical in meaning to the factor UF used here, of 1.0. Thus, final values of CVN given here are higher than those given in Ref. 17.

In order to remedy the existing dependence on possibly unrepresentative data for estimating the J-R curves for A302-B steel, the Oak Ridge National Laboratory is beginning a testing program under NRC sponsorship to obtain data from actual vessel archive A302-B materials.[32] Tests will be performed on specimens ranging in size from 1/2T to 4T, from seven different plates, over a temperature range from 180°F to 550°F, and for three different specimen orientations, TL, LS, and LT.[32] These data will facilitate more realistic estimates of J-R curves for A302-B steel, probably leading to less restrictive conclusions than have been reached to date.

CONCLUSIONS

Technical Conclusions

1. The NMPC J-R curve estimate for A302-B plate is not a -2σ estimate, as stated in Ref. 1. This is because only the J_{Ic} portion of the estimate has that statistical significance. The ΔJ

portion of the estimate is based on either mean data or data from a single specimen.

2. The experimental J-R curve for A302-B TL specimen V50-101 tested at 180°F is virtually flat, for $\Delta a > 0.10$ in. A power law fits the experimental J-R curve up to $\Delta a = 0.10$ in., but not beyond.

3. A modification of the J-R curve estimating procedure for high sulfur A302-B steel recommended in Ref. 26, whereby J = const. for $\Delta a > 0.10$ in., has been chosen by the writers as the basis for independent checking calculations for NMP-1.

4. Checking calculations made according to ASME Section XI Code Case N-512 and the writers' J-R curve estimates indicate that the NMP-1 plates satisfy Level A and B criteria, but not by the margins indicated in Ref. 1.

5. Checking calculations performed with the FAVOR code indicate that Level C and D conditions do not govern. If the more conservative requirements of Ref. 13 are applied, the required LT CVN value for Level D is satisfied by the same margin as for Levels A and B.

Regulatory Conclusions

The low upper-shelf analyses for Nine Mile Point Unit 1 (NMP-1) for Levels A, B, C, and D submitted by Niagara Mohawk Power Corporation have been reviewed. A basic deficiency in the analyses has been identified, namely an incomplete allowance for the random variability of J-R curves. Nevertheless, independent checking calculations with a reasonable estimate of statistical variability effects indicate that the NMP-1 reactor pressure vessel meets the low upper-shelf safety margin criteria prescribed by ASME Section XI, Code Case N-512.

[illegible]

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Table 1

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

Nine Mile Point 1
Oyster Creek 1
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

Summary of USE Ratio Results for A302-B and A533-B Plates

Material Type	I/U	No. of Sets	Avg. Ratio	Sd. Dev.	Min.	Max.
A 302-B Plate	U	10	0.74	0.11	0.62	0.97
A 302-B Plate	I	6	0.73	0.06	0.60	0.78
A 533-B Plate	U	23	0.74	0.08	0.56	0.88
A 533-B Plate	I	27	0.75	0.06	0.57	0.86

Table 5

Effect of ΔJ Variation on the Minimum Upper Shelf Energy Level for NMP-1 Plate G-8-1 (NMPC Calculations)

Plate	ASME Service Level	Material Model	Minimum USE (Ft-lbs.) $\Delta J = 175$ in-lb/in.^{2}		Minimum USE (Ft-lbs.) Variable ΔJ	
			Flaw Growth of 0.1 in. Criterion $J_1 < J_{0.1}$	Flaw Stability Criterion	Flaw Growth of 0.1 in. Criterion $J_1 < J_{0.1}$	Flaw Stability Criterion
G-8-1	A&B	A302B	13	23	33	36
G-8-1	C	A302B	10	10	31	31
G-8-1	D	A302B	n/a	20	n/a	30

Table 6

Effect of Clad Stress on the Minimum Upper Shelf Energy Level for NMP-1 Plate G-8-1 (NMPC Calculations)

Plate	ASME Service Level	Material Model	Minimum USE (Ft-Lbs) Without Clad Stress Effect		Minimum USE (Ft-Lbs.) With Clad Stress Effect	
			Flaw Growth of 0.1 in. Criterion $J_1 < J_{0.1}$	Flaw Stability Criterion	Flaw Growth of 0.1 in. Criterion $J_1 < J_{0.1}$	Flaw Stability Criterion
G-8-1	A&B	A302B	13	23	26	37

Table 7

Input Values Used for Level A and B Checking Calculations by Code Case N-512 Procedures

Modulus of elasticity	:	$E = 26{,}400$ ksi @ 500°F (NMP estimate)
Poisson's ratio	:	$\nu = 0.33$
Base metal thickness	:	$t_b = 7.125$ in. (See Note 1)
Clad thickness	:	$t_{cl} = 0.156$ in.
Flaw depth for Criterion #1	:	$a = \frac{t_b + t_{cl}}{4} + 0.10 = 1.92$in.
Design pressure	:	$P_d = 1.25$ ksi
Accumulation pressure	:	$P_{acc} = 1.10\ P_d = 1.375$ ksi
Cooling rate	:	CR = 100°F/hr.

Note 1. The value of t_b listed here is slightly incorrect due to error prone nomenclature. It was calculated as wall thickness minus clad thickness (7.281 – 0.156 = 7.125 in.). But the common term wall thickness does not include cladding. See Table 8. Resulting errors are expected to not be significant in terms of safety margins when Levels A and B govern.

Table 8

NMP-1 Vessel Diminensions

Inside radis of cladding	:	$R_{i(cl)}$	= 106.344 in.
Cladding thickness	:	t_{cl}	= 0.1563 in.
Inside radius of base metal	:	$R_{i(bm)}$	= 106.5 in.
Base metal wall thickness	:	t_b	= 7.281 in.
Outside radius	:	R_o	= 113.781 in.

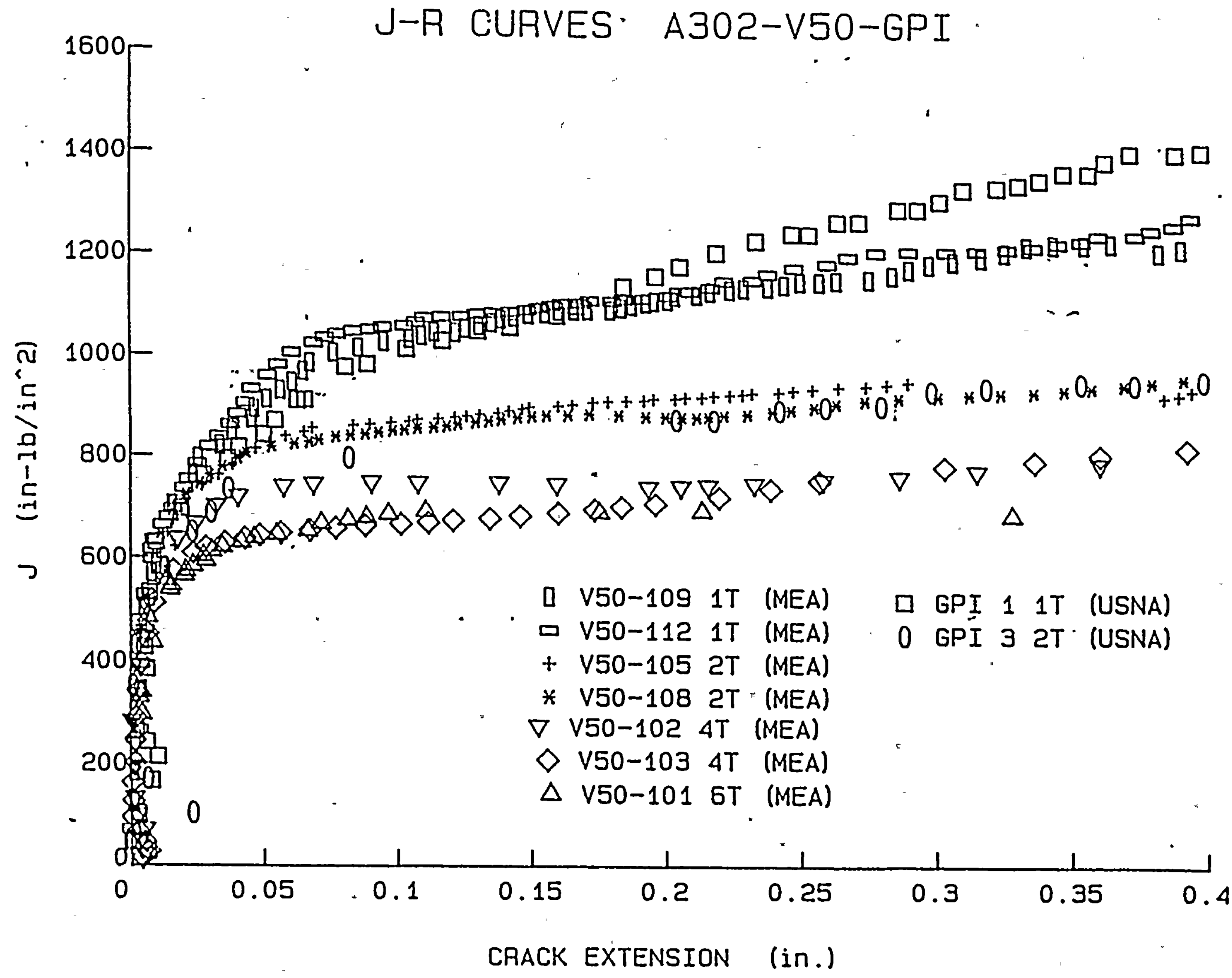

J-R CURVES A302-V50-GPI
J (in-lb/in^2)
1600
1400
1200
1000
800
600
400
200
0
0
0.05
0.1
0.15
0.2
0.25
0.3
0.35
0.4
CRACK EXTENSION (in.)
V50-109 1T (MEA)
V50-112 1T (MEA)
V50-105 2T (MEA)
V50-108 2T (MEA)
V50-102 4T (MEA)
V50-103 4T (MEA)
V50-101 6T (MEA)
GPI 1 1T (USNA)
GPI 3 2T (USNA)

FIGURE 1

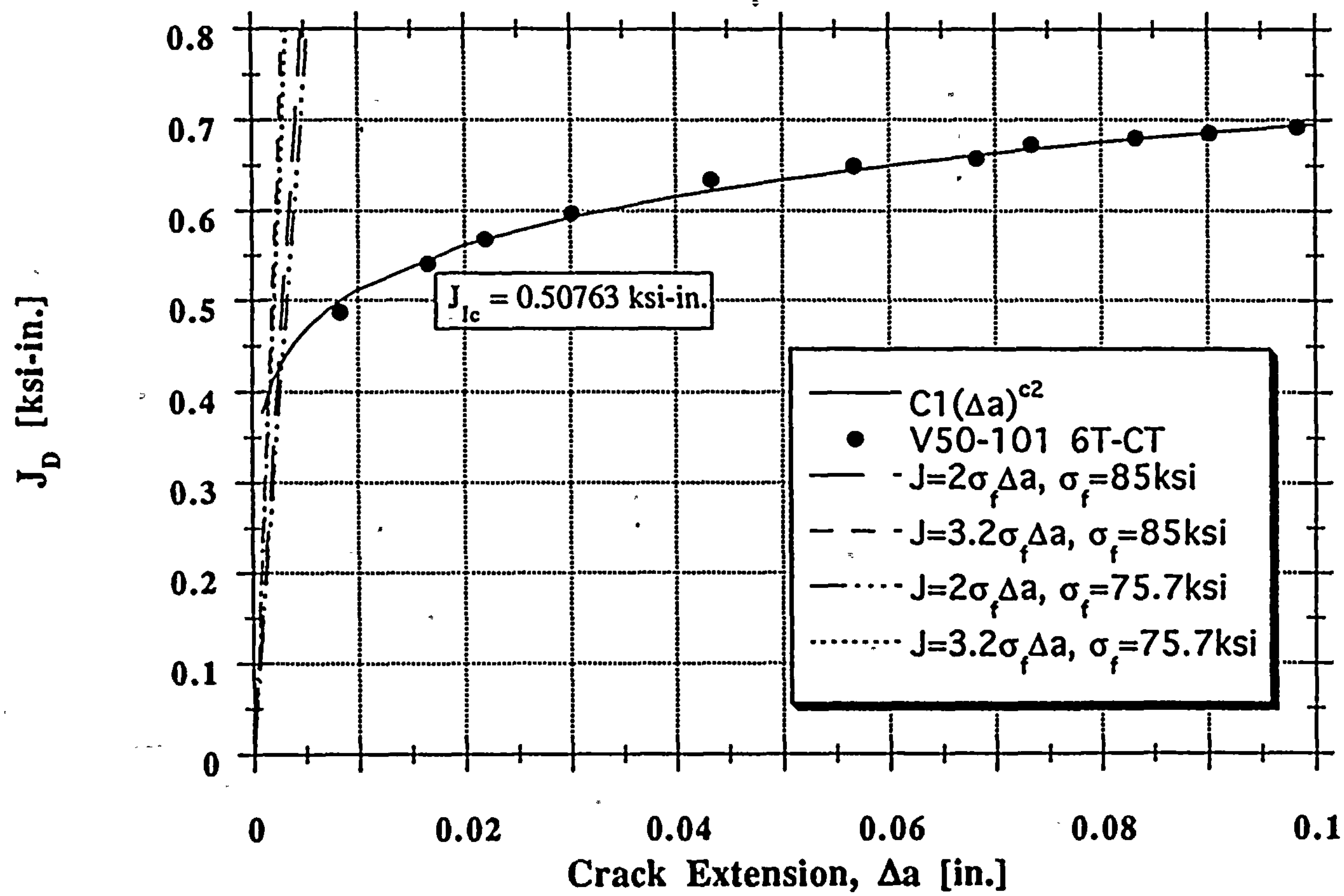
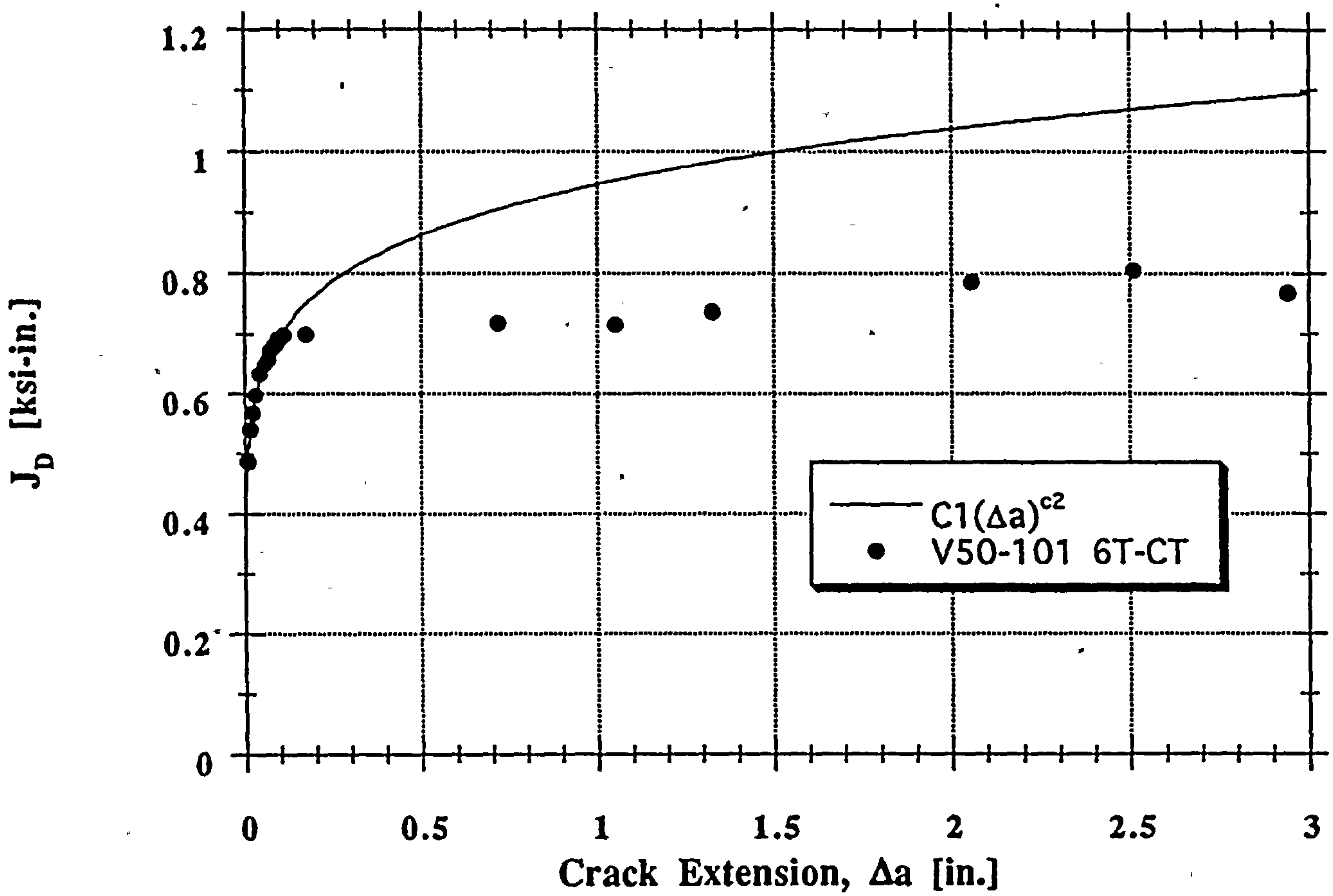

0.8
0.7
0.6
0.5
0.4
0.3
0.2
0.1
0
J_D [ksi-in.]
0
0.02
0.04
0.06
0.08
0.1
Crack Extension, Δa [in.]
J_Ic = 0.50763 ksi-in.
C1(Δa)^c2
V50-101 6T-CT
J=2σ_fΔa, σ_f=85ksi
J=3.2σ_fΔa, σ_f=85ksi
J=2σ_fΔa, σ_f=75.7ksi
J=3.2σ_fΔa, σ_f=75.7ksi

Fig 2.



1.2
1
0.8
0.6
0.4
0.2
0
J_D [ksi-in.]
0
0.5
1
1.5
2
2.5
3
Crack Extension, Δa [in.]
C1(Δa)^c2
V50-101 6T-CT

Fig. 3

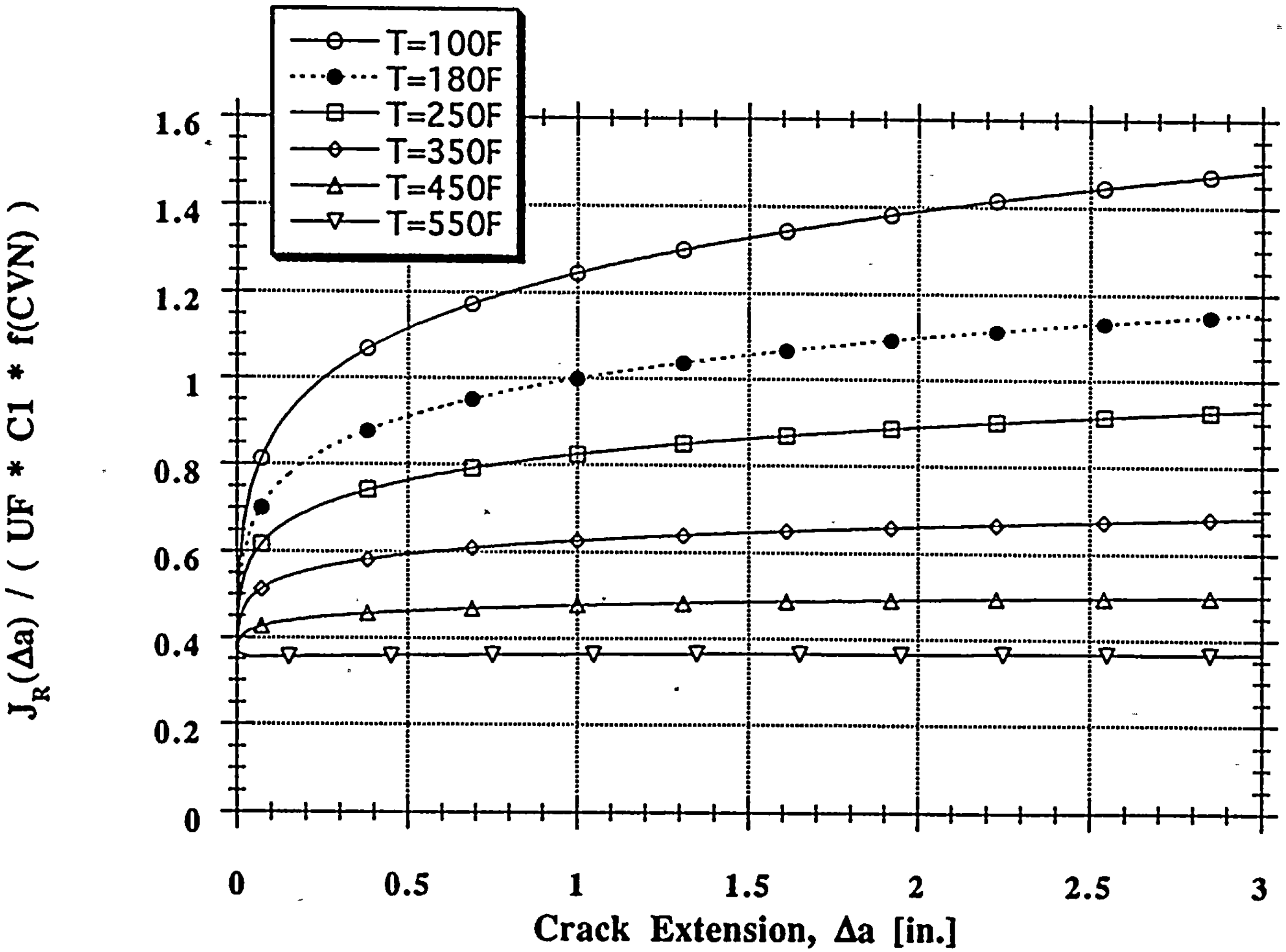
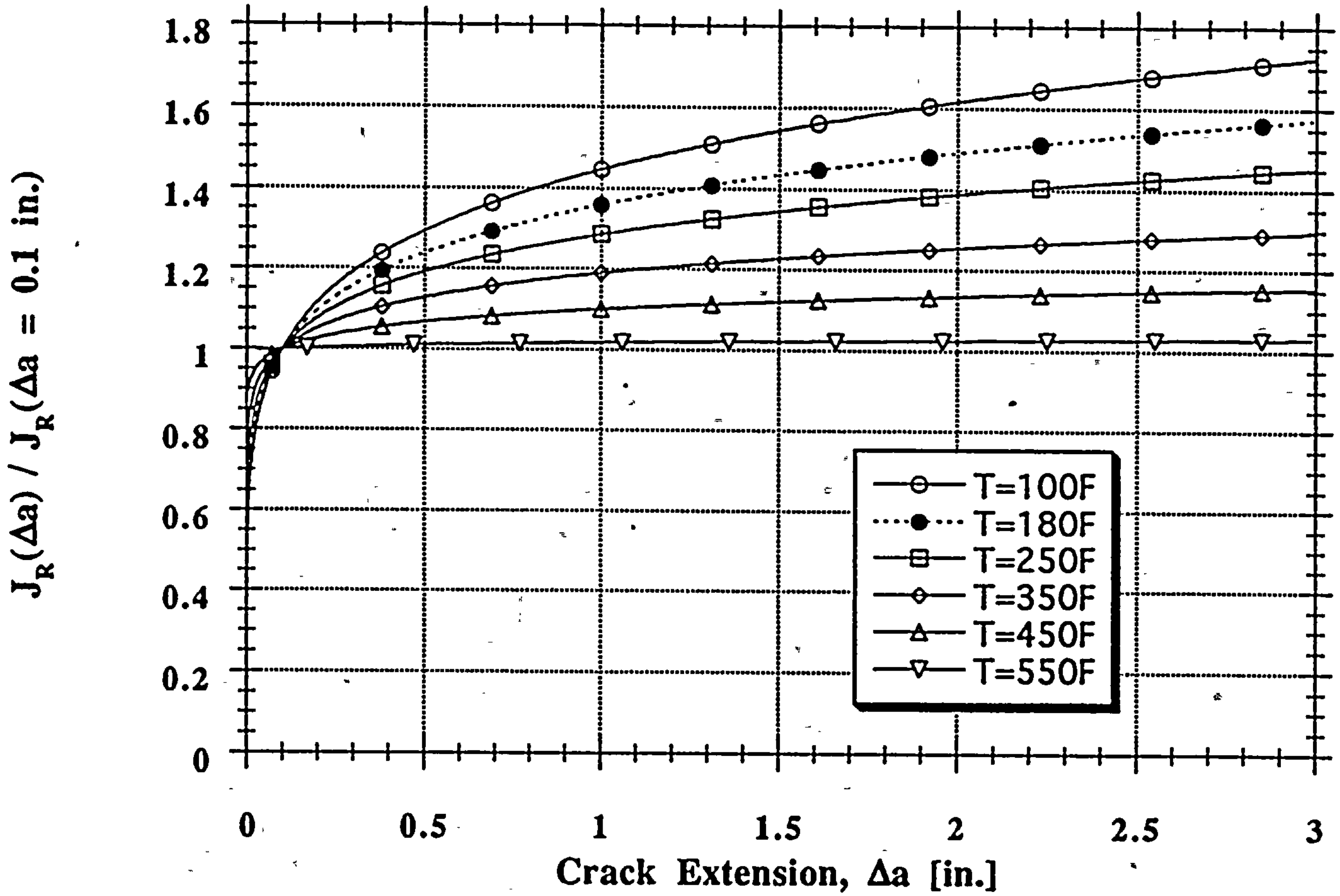

T=100F
T=180F
T=250F
T=350F
T=450F
T=550F
1.6
1.4
1.2
1
0.8
0.6
0.4
0.2
0
0
0.5
1
1.5
2
2.5
3
Crack Extension, Δa [in.]
J_R(Δa) / (UF * C1 * f(CVN))

Fig. 4



1.8
1.6
1.4
1.2
1
0.8
0.6
0.4
0.2
0
0
0.5
1
1.5
2
2.5
3
$J_R(\Delta a) / J_R(\Delta a = 0.1 \text{ in.})$
Crack Extension, Δa [in.]
T=100F
T=180F
T=250F
T=350F
T=450F
T=550F

Fig 5.

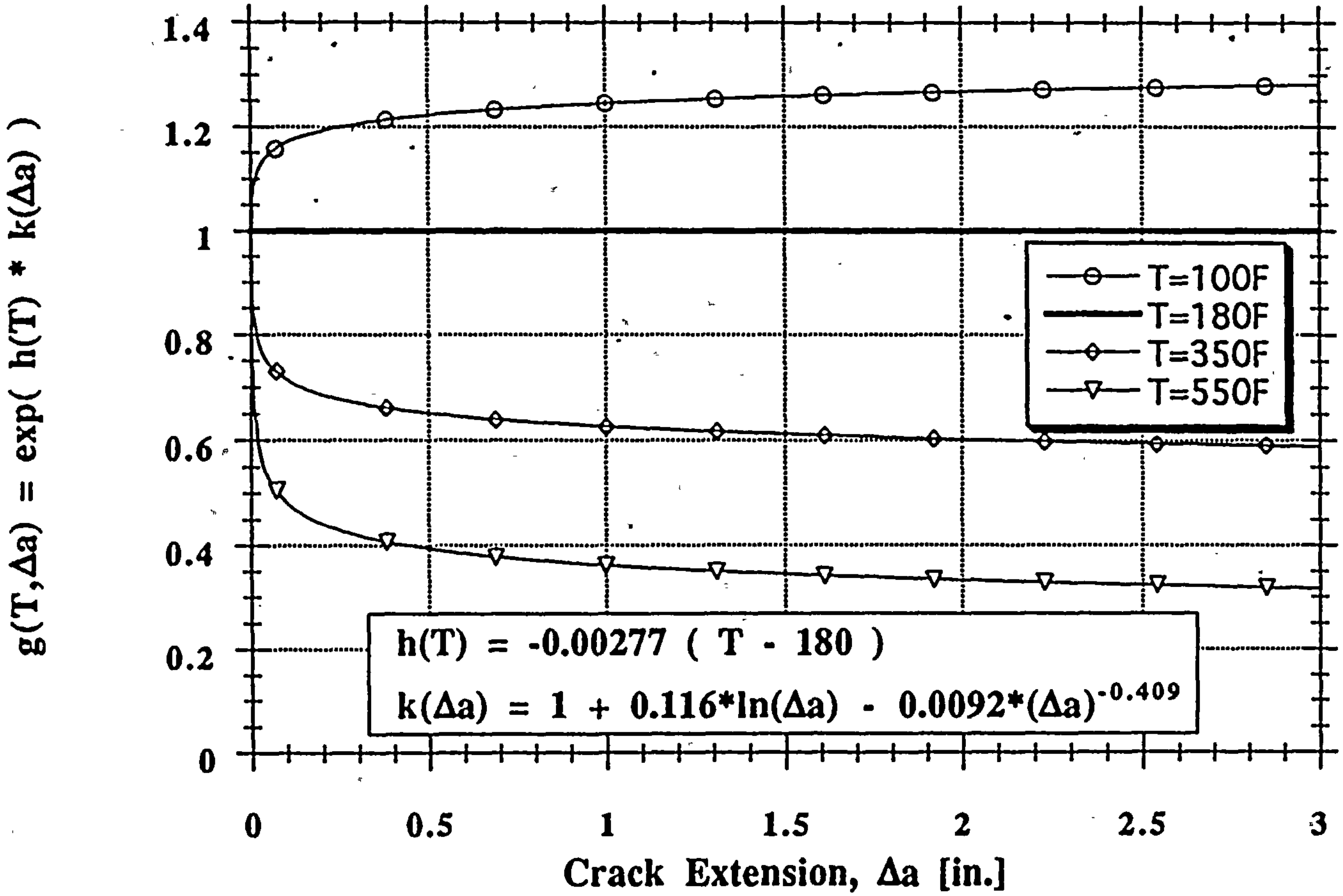

1.4
1.2
1
0.8
0.6
0.4
0.2
0
g(T,Δa) = exp(h(T) * k(Δa))
T=100F
T=180F
T=350F
T=550F
h(T) = -0.00277 (T - 180)
k(Δa) = 1 + 0.116*ln(Δa) - 0.0092*(Δa)^-0.409
0
0.5
1
1.5
2
2.5
3
Crack Extension, Δa [in.]

Fig. 6

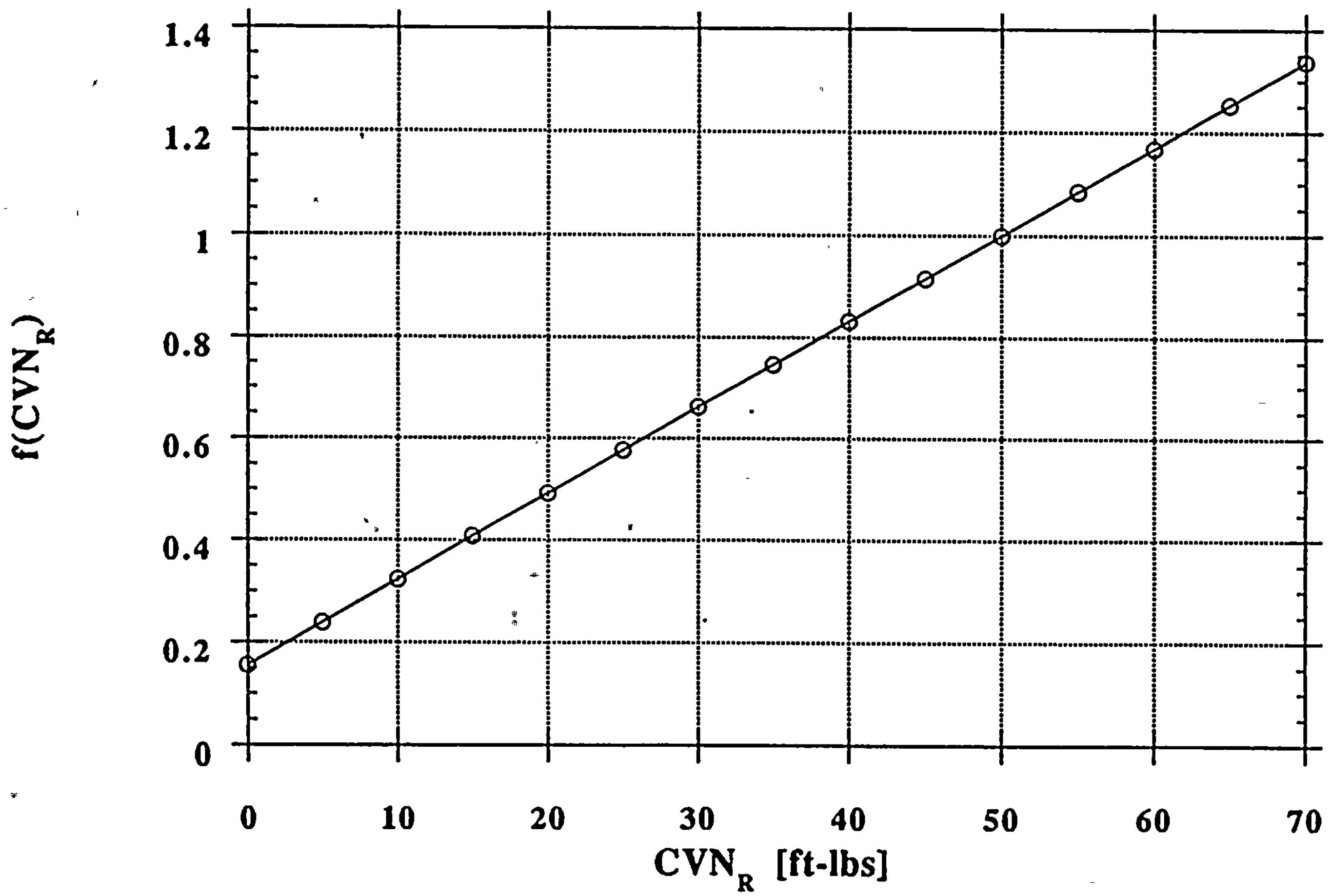


Fig. 7

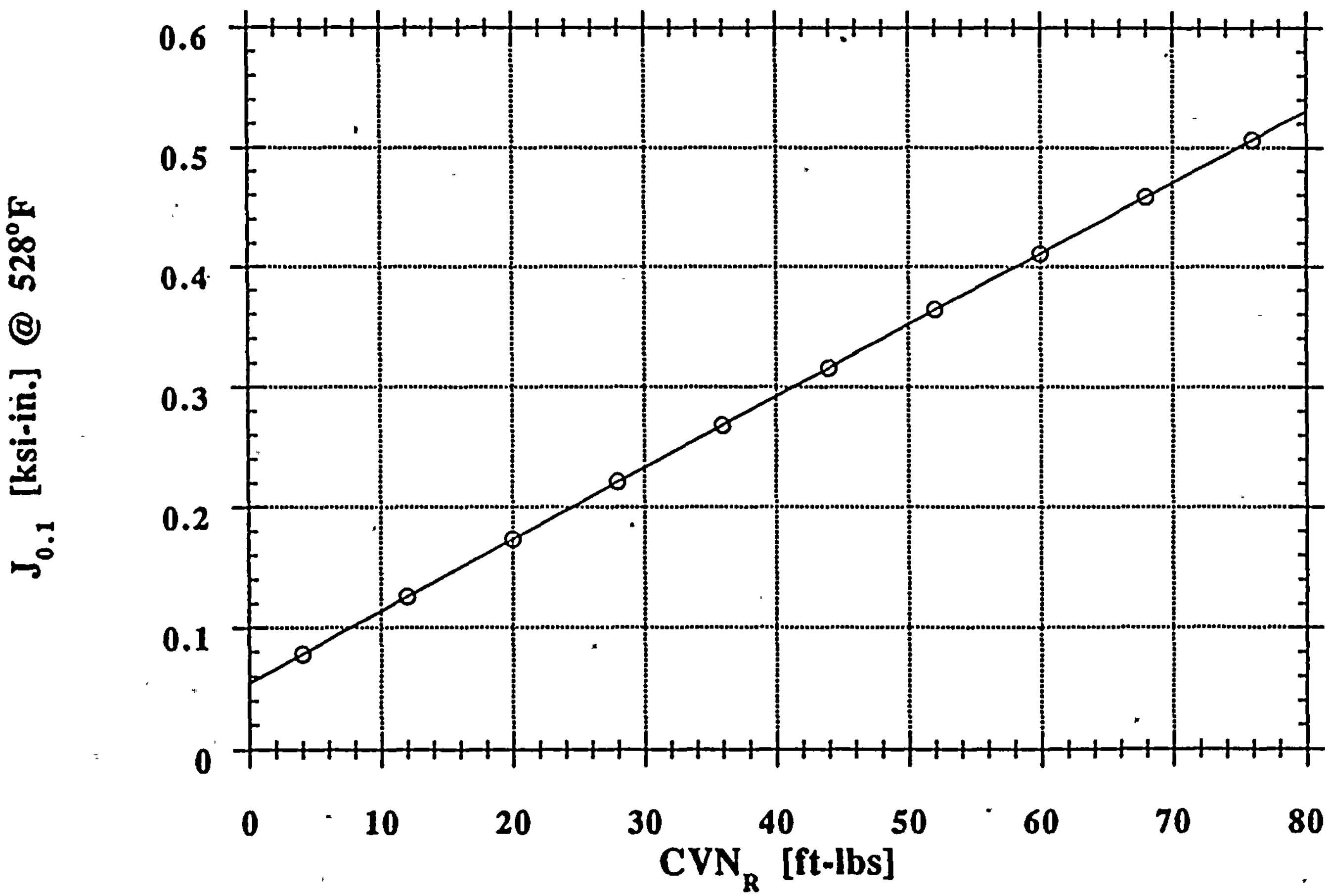
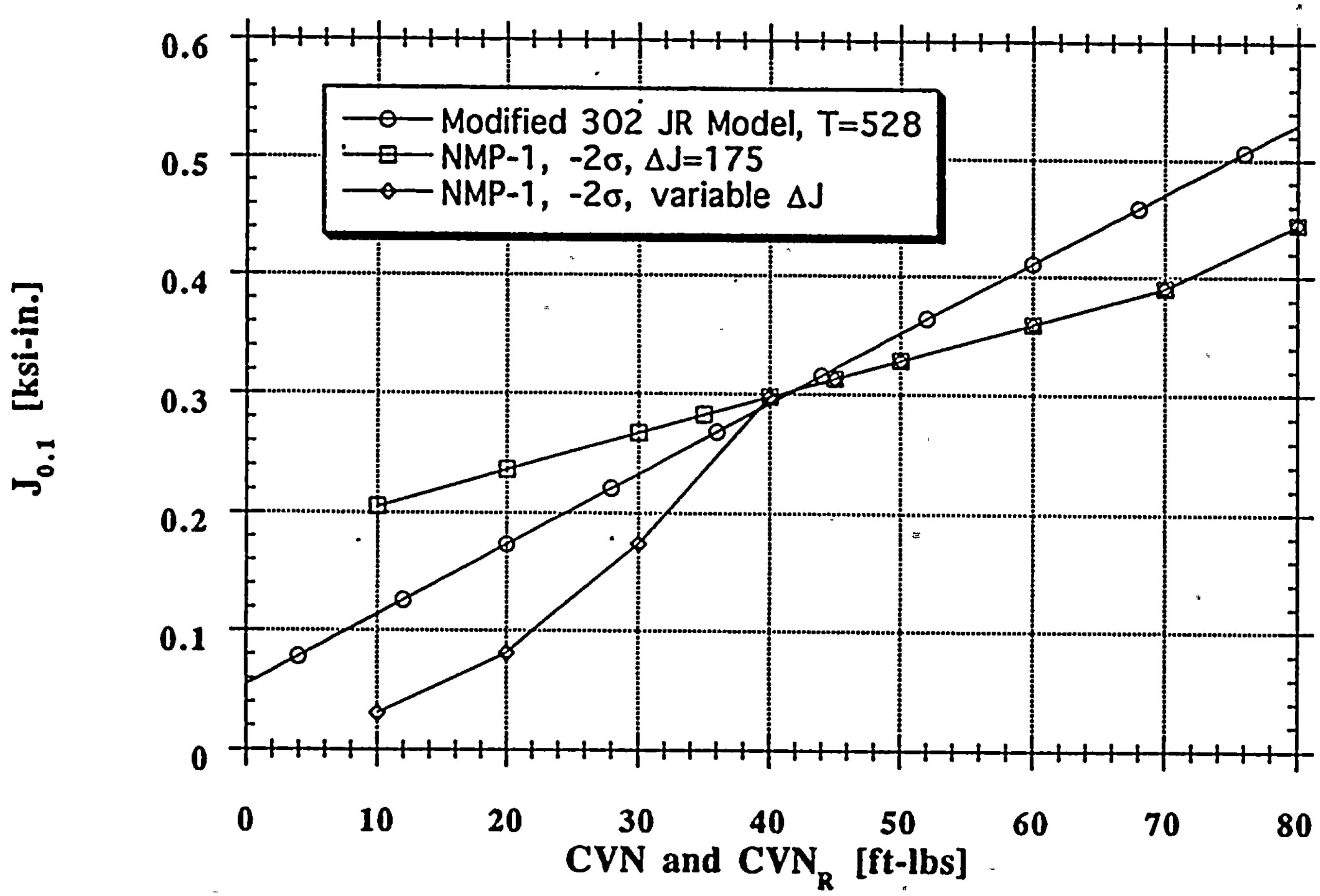
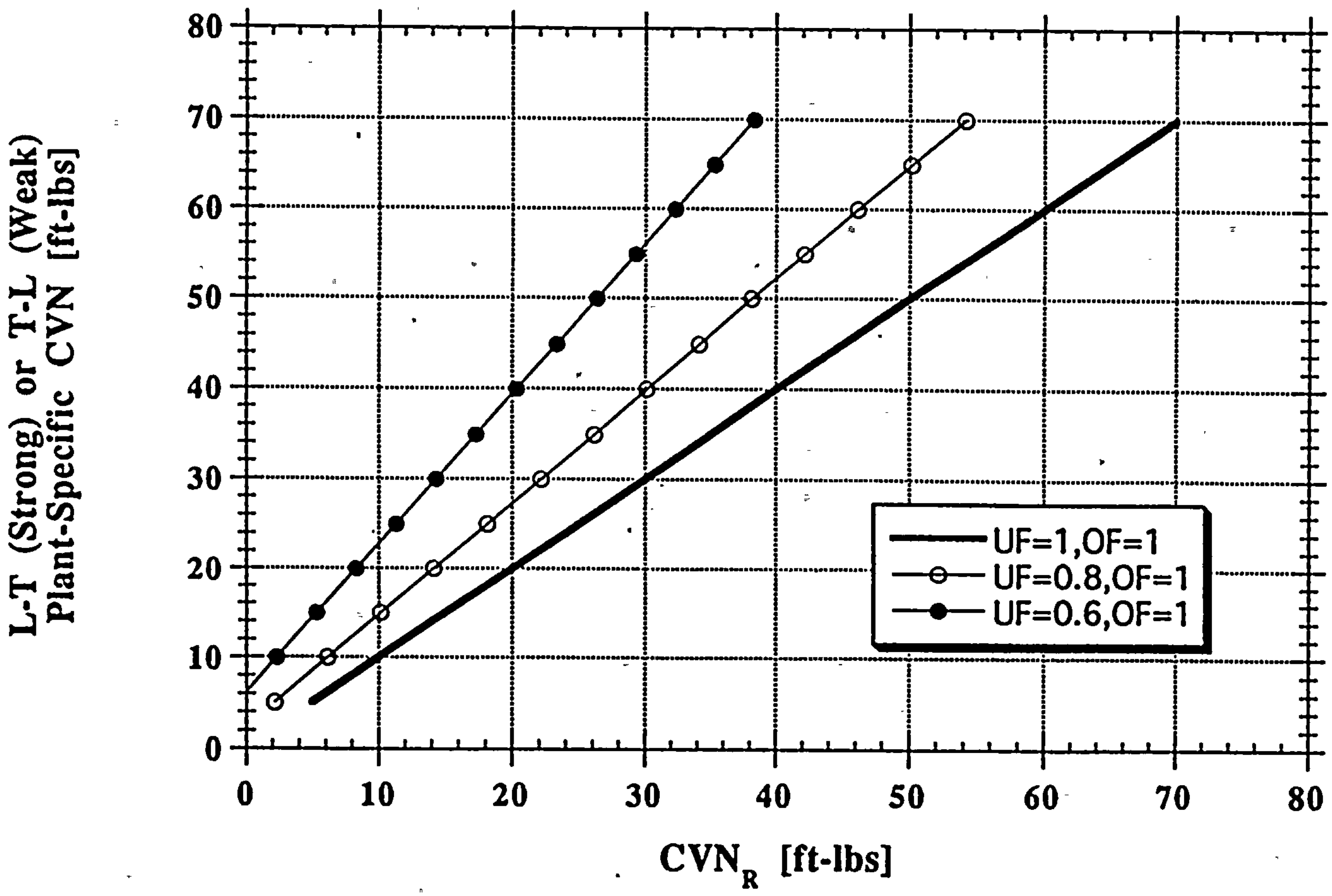

0.6
0.5
0.4
0.3
0.2
0.1
0
$J_{0.1}$ [ksi-in.] @ 528°F
0
10
20
30
40
50
60
70
80
CVN_R [ft-lbs]

Fig. 8



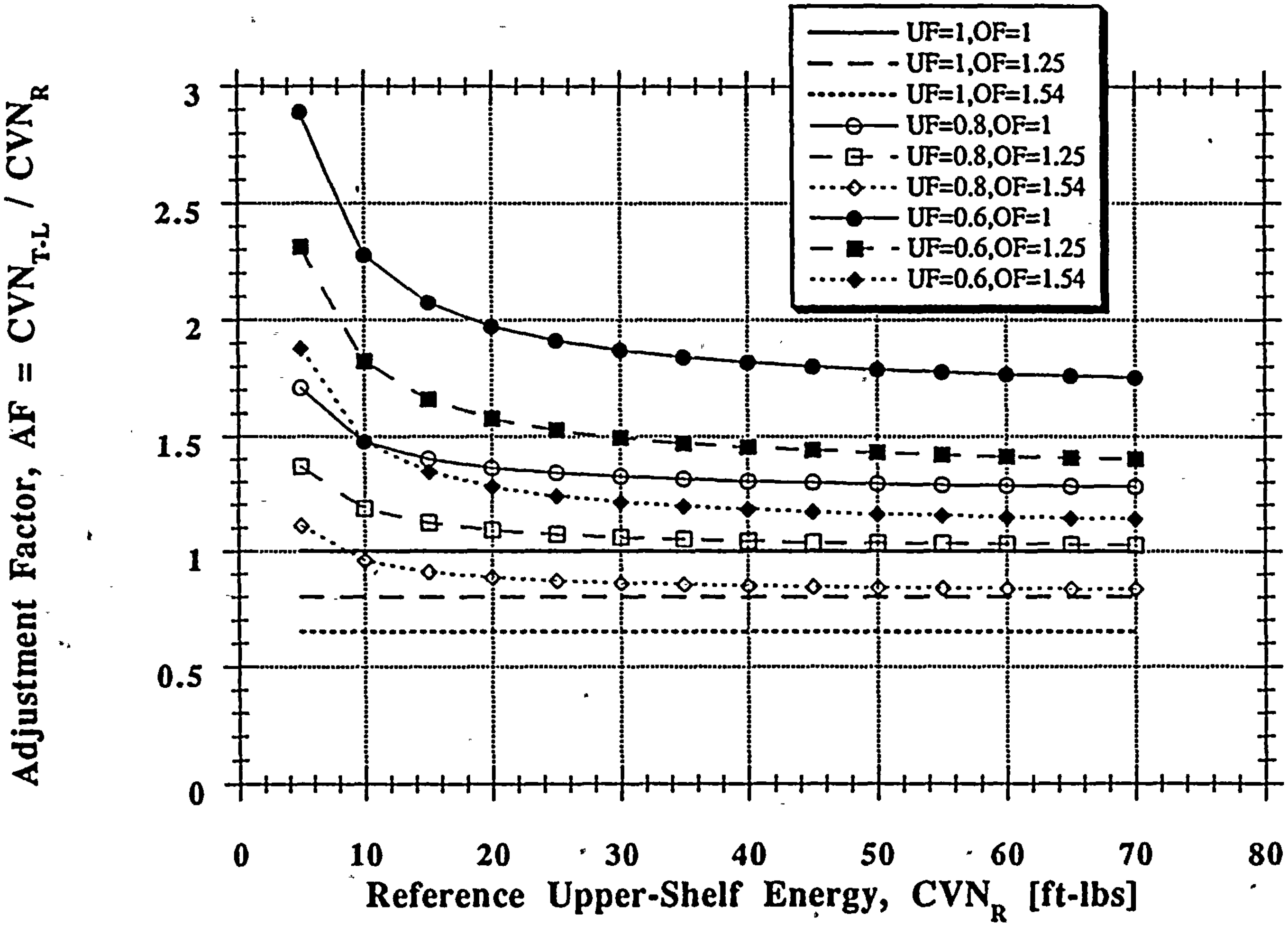
Modified 302 JR Model, T=528
NMP-1, -2σ, ΔJ=175
NMP-1, -2σ, variable ΔJ
0.6
0.5
0.4
0.3
0.2
0.1
0
J0.1 [ksi-in.]
0
10
20
30
40
50
60
70
80
CVN and CVNR [ft-lbs]

Fig 9



L-T (Strong) or T-L (Weak)
Plant-Specific CVN [ft-lbs]
CVN_R [ft-lbs]
UF=1,OF=1
UF=0.8,OF=1
UF=0.6,OF=1
0
10
20
30
40
50
60
70
80

Fig. 10



UF=1,OF=1
UF=1,OF=1.25
UF=1,OF=1.54
UF=0.8,OF=1
UF=0.8,OF=1.25
UF=0.8,OF=1.54
UF=0.6,OF=1
UF=0.6,OF=1.25
UF=0.6,OF=1.54
Adjustment Factor, AF = CVN_T-L / CVN_R
Reference Upper-Shelf Energy, CVN_R [ft-lbs]
0 0.5 1 1.5 2 2.5 3
0 10 20 30 40 50 60 70 80

Fig. 11

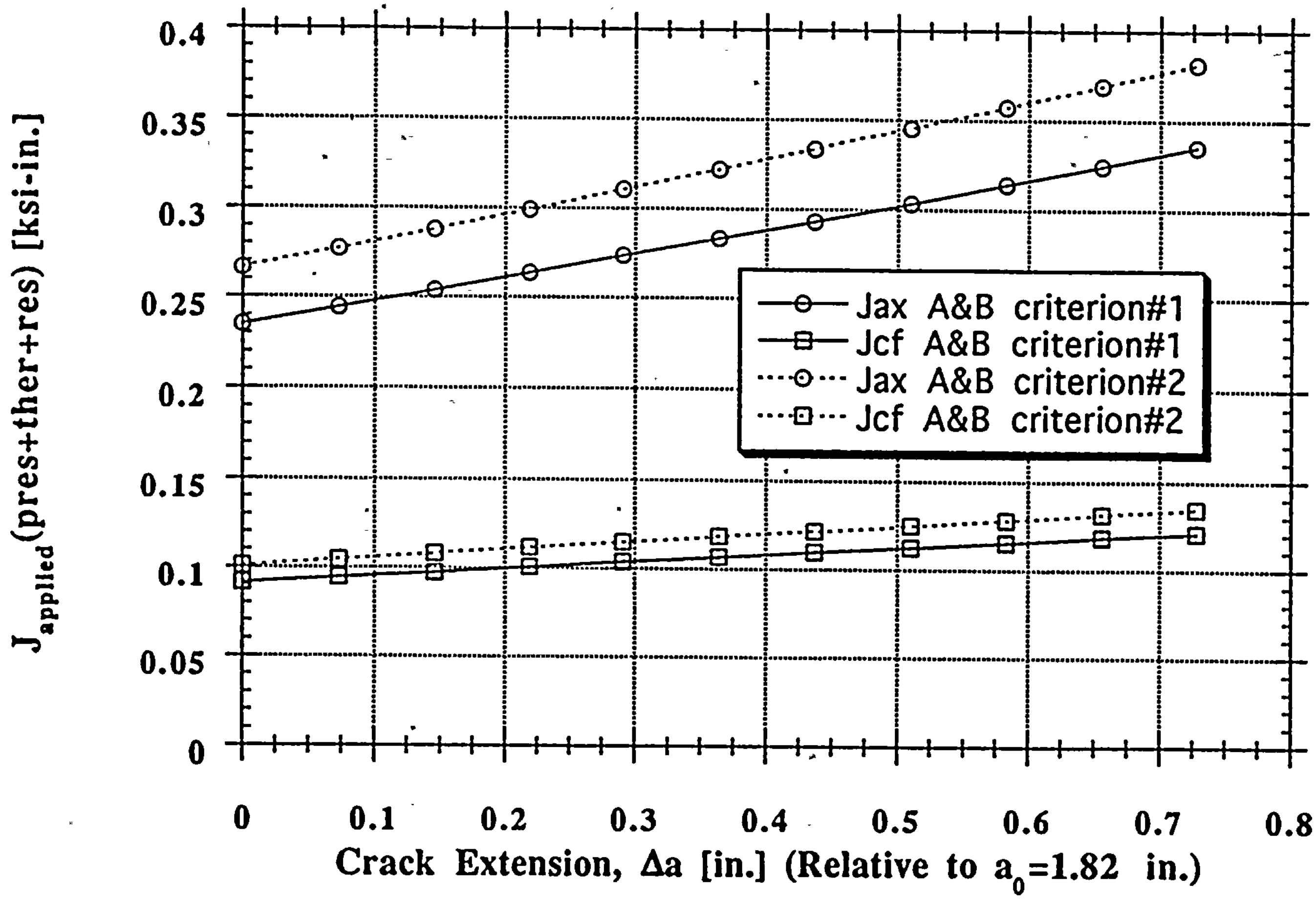
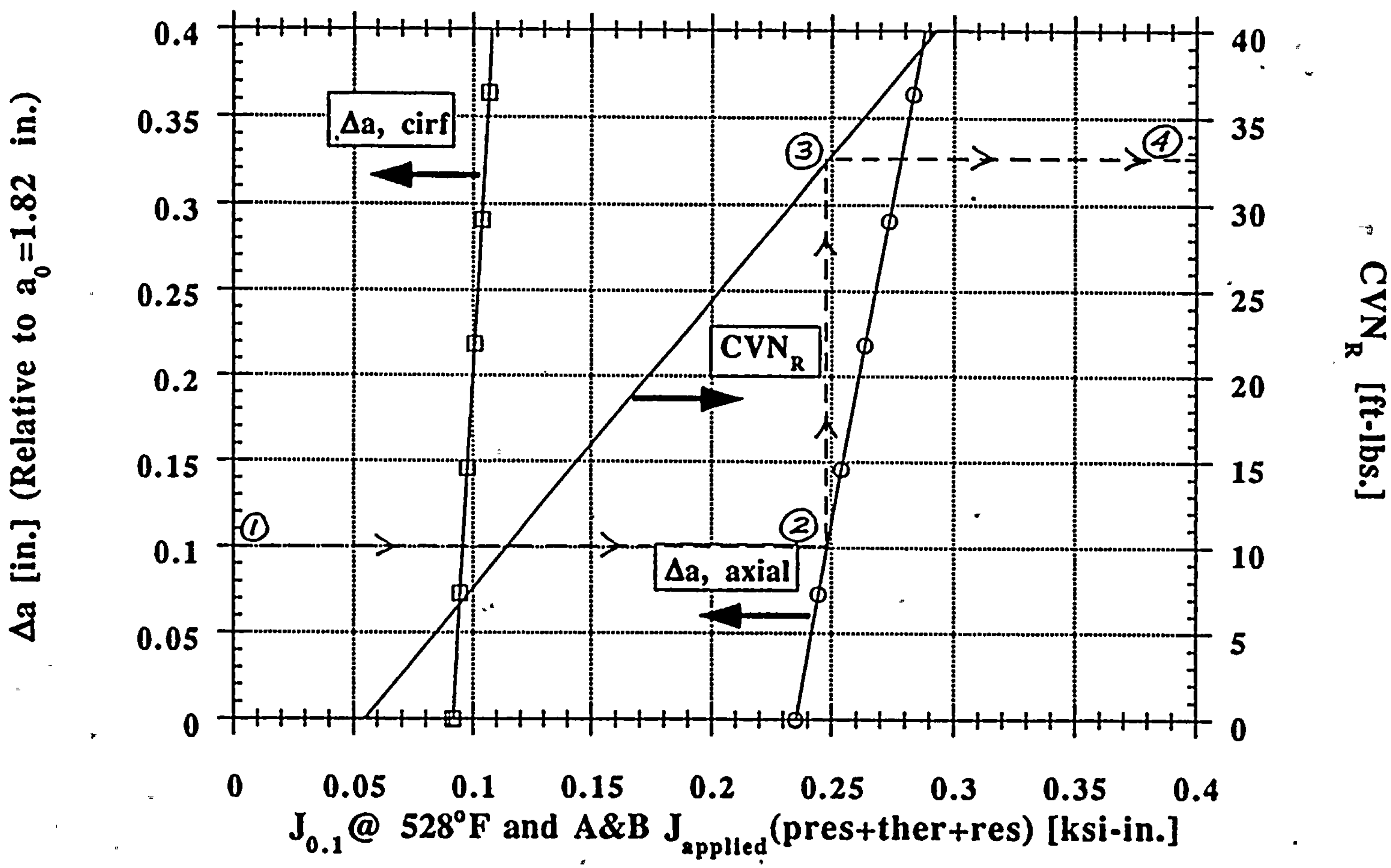
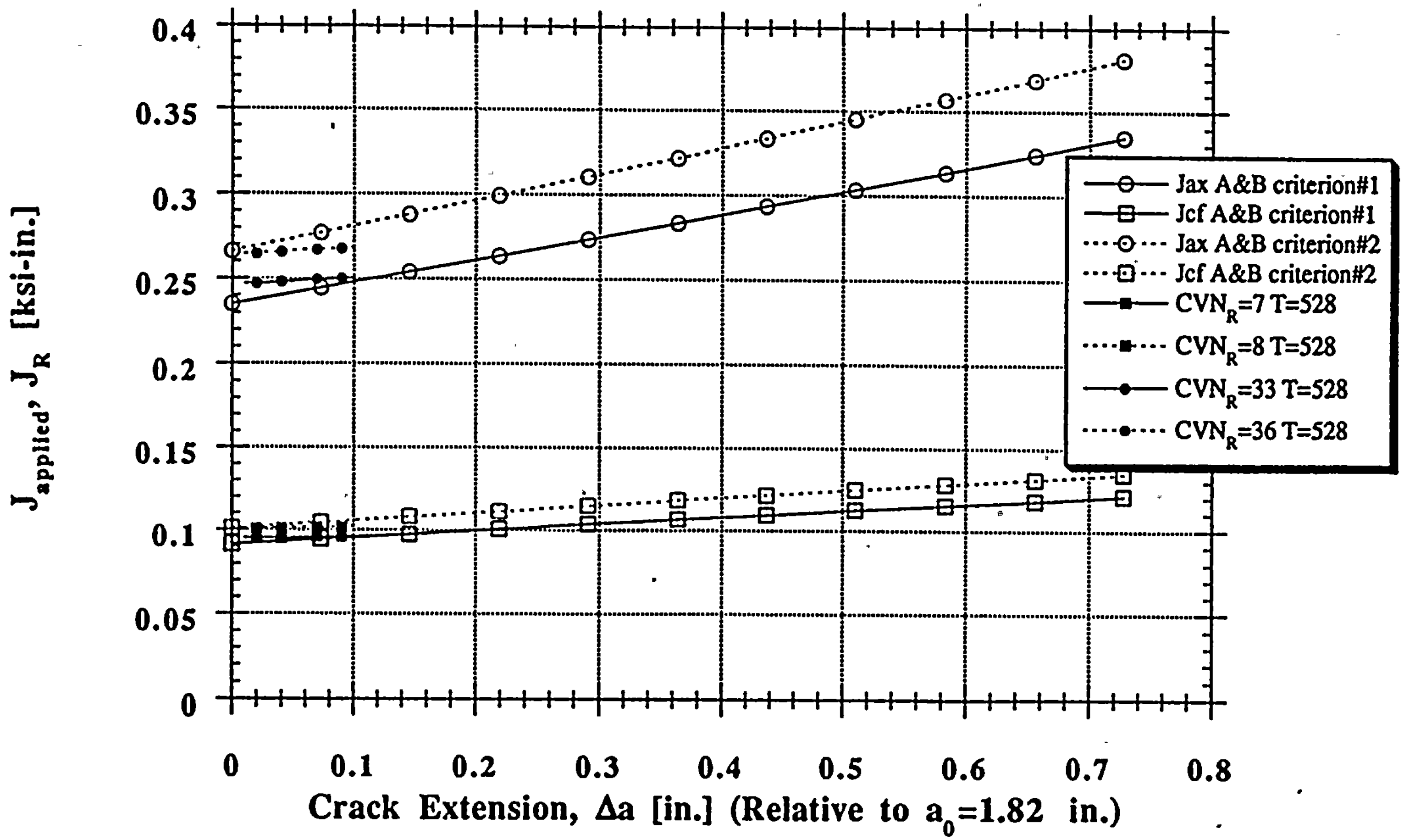

0.4
0.35
0.3
0.25
0.2
0.15
0.1
0.05
0
$J_{applied}$(pres+ther+res) [ksi-in.]
0
0.1
0.2
0.3
0.4
0.5
0.6
0.7
0.8
Crack Extension, Δa [in.] (Relative to a_0=1.82 in.)
Jax A&B criterion#1
Jcf A&B criterion#1
Jax A&B criterion#2
Jcf A&B criterion#2

Fig. 12



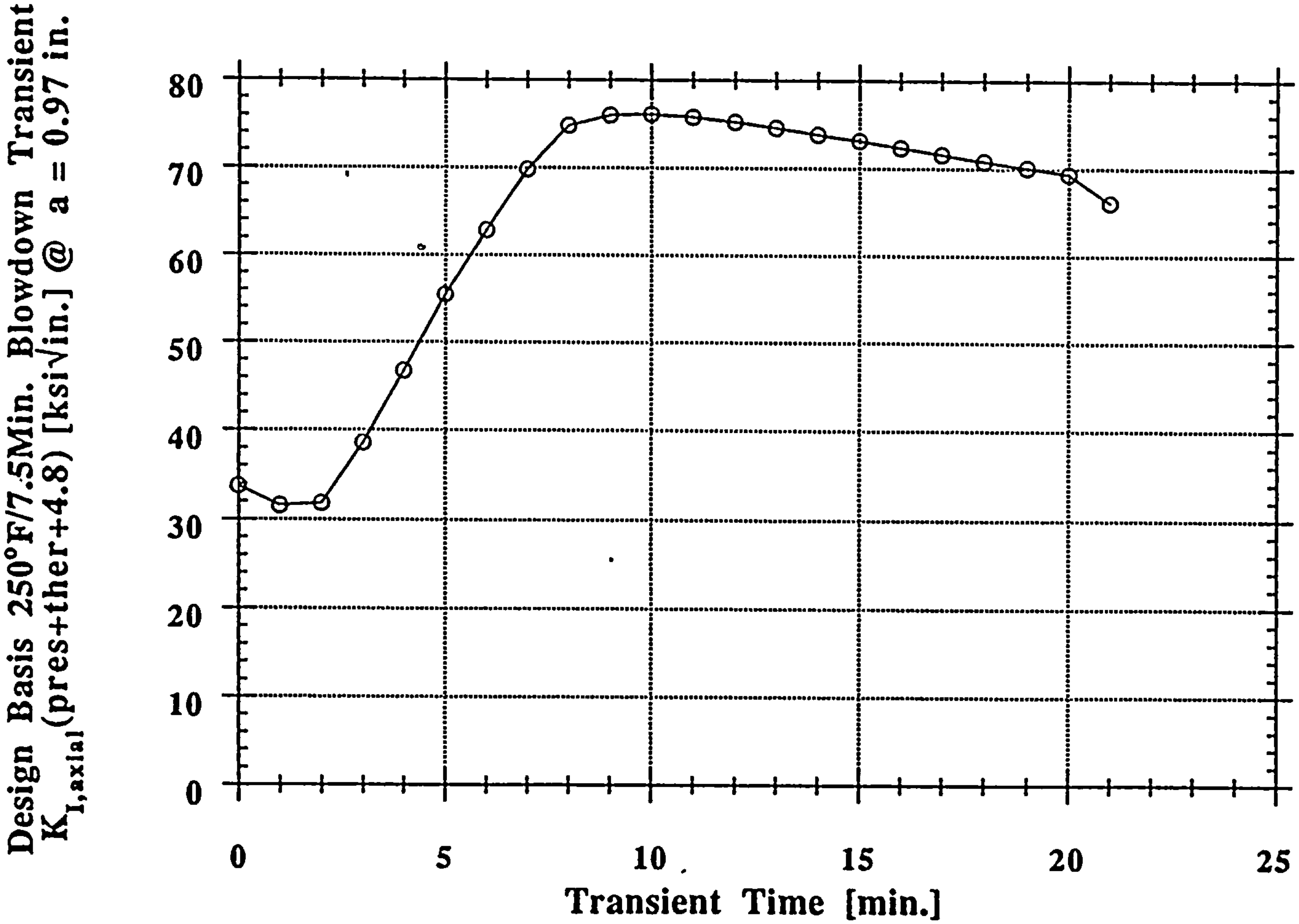
Δa [in.] (Relative to a0=1.82 in.)
CVNR [ft-lbs.]
J0.1@ 528°F and A&B Japplied(pres+ther+res) [ksi-in.]
Δa, cirf
CVNR
Δa, axial
0
0.05
0.1
0.15
0.2
0.25
0.3
0.35
0.4
5
10
15
20
25
30
35
40

Fig. 13



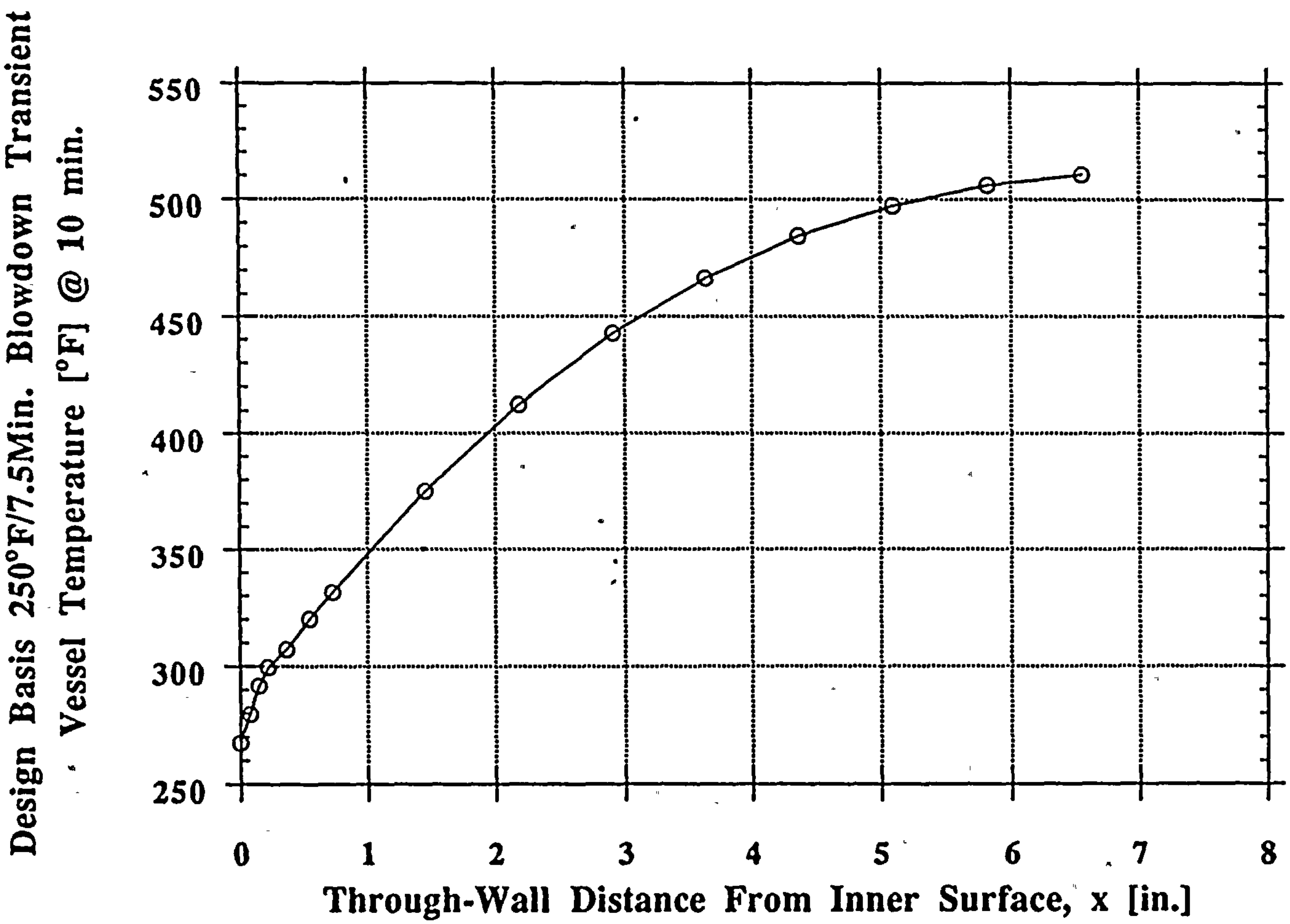
0.4
0.35
0.3
0.25
0.2
0.15
0.1
0.05
0
0
0.1
0.2
0.3
0.4
0.5
0.6
0.7
0.8
$J_{applied}$, J_R [ksi-in.]
Crack Extension, Δa [in.] (Relative to a_0=1.82 in.)
Jax A&B criterion#1
Jcf A&B criterion#1
Jax A&B criterion#2
Jcf A&B criterion#2
CVN_R=7 T=528
CVN_R=8 T=528
CVN_R=33 T=528
CVN_R=36 T=528

Fig. 14



Design Basis 250°F/7.5Min. Blowdown Transient
K_I,axial(pres+ther+4.8) [ksi√in.] @ a = 0.97 in.
Transient Time [min.]
0
5
10
15
20
25
80
70
60
50
40
30
20
10
0

Fig. 15



Design Basis 250°F/7.5Min. Blowdown Transient
Vessel Temperature [°F] @ 10 min.
550
500
450
400
350
300
250
0
1
2
3
4
5
6
7
8
Through-Wall Distance From Inner Surface, x [in.]

Fig. 16

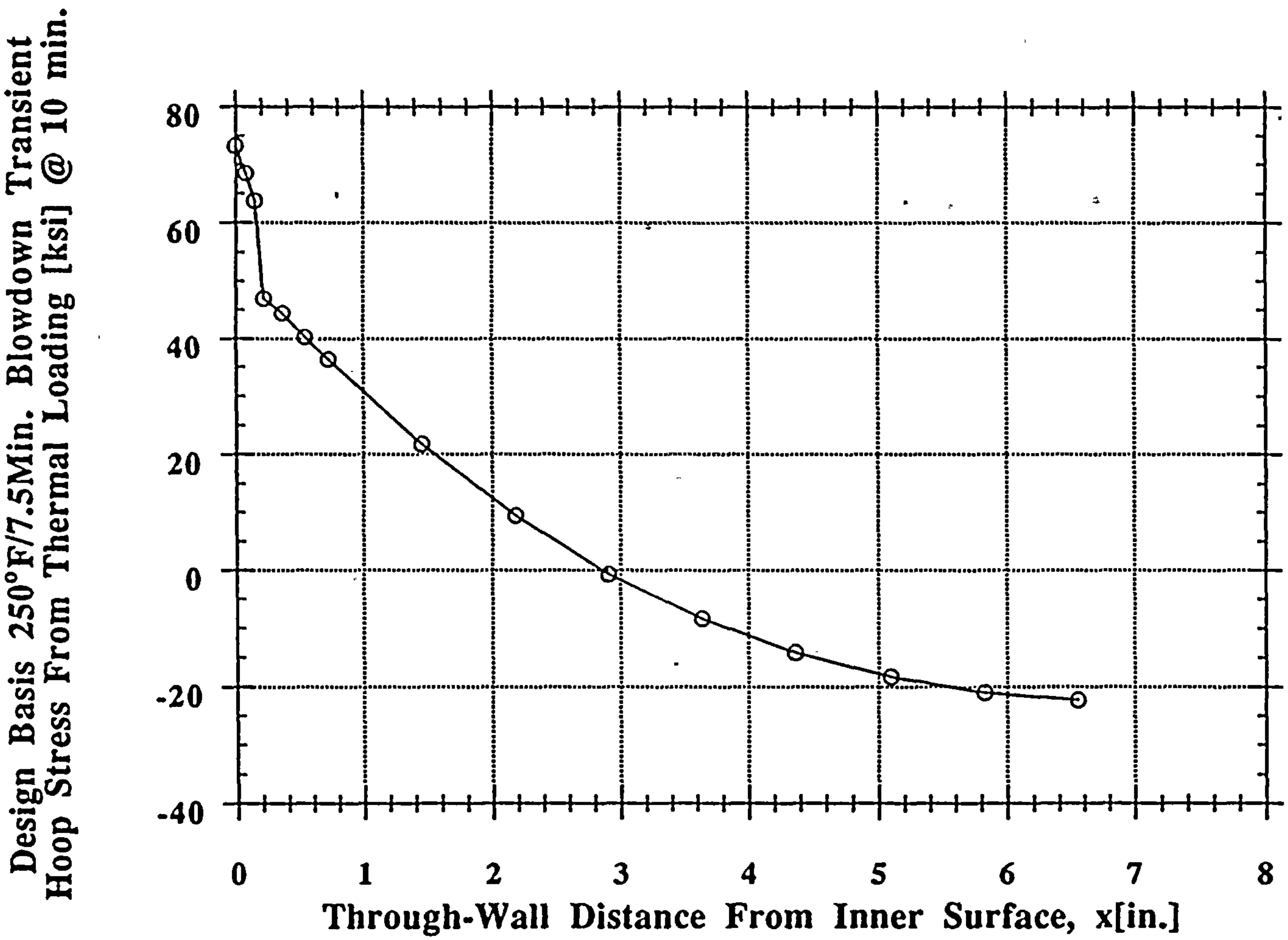
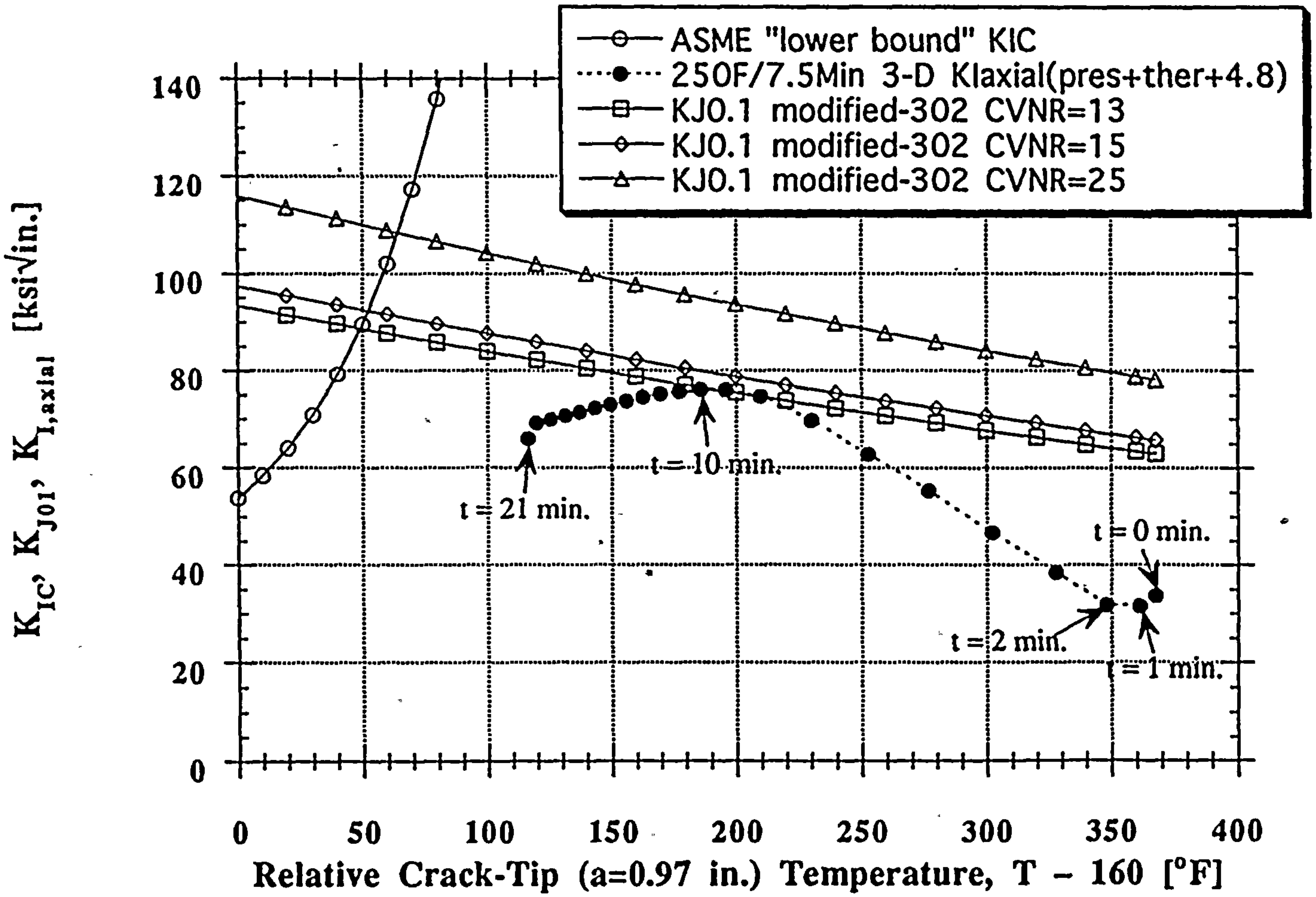

Design Basis 250°F/7.5Min. Blowdown Transient
Hoop Stress From Thermal Loading [ksi] @ 10 min.
80
60
40
20
0
-20
-40
0
1
2
3
4
5
6
7
8
Through-Wall Distance From Inner Surface, x[in.]

Fig. 17



ASME "lower bound" KIC
250F/7.5Min 3-D KIaxial(pres+ther+4.8)
KJ0.1 modified-302 CVNR=13
KJ0.1 modified-302 CVNR=15
KJ0.1 modified-302 CVNR=25
K_{IC}, K_{J01}, $K_{I,axial}$ [ksi√in.]
Relative Crack-Tip (a=0.97 in.) Temperature, T − 160 [°F]
t = 21 min.
t = 10 min.
t = 0 min.
t = 2 min.
t = 1 min.
0
20
40
60
80
100
120
140
0
50
100
150
200
250
300
350
400

Fig 18

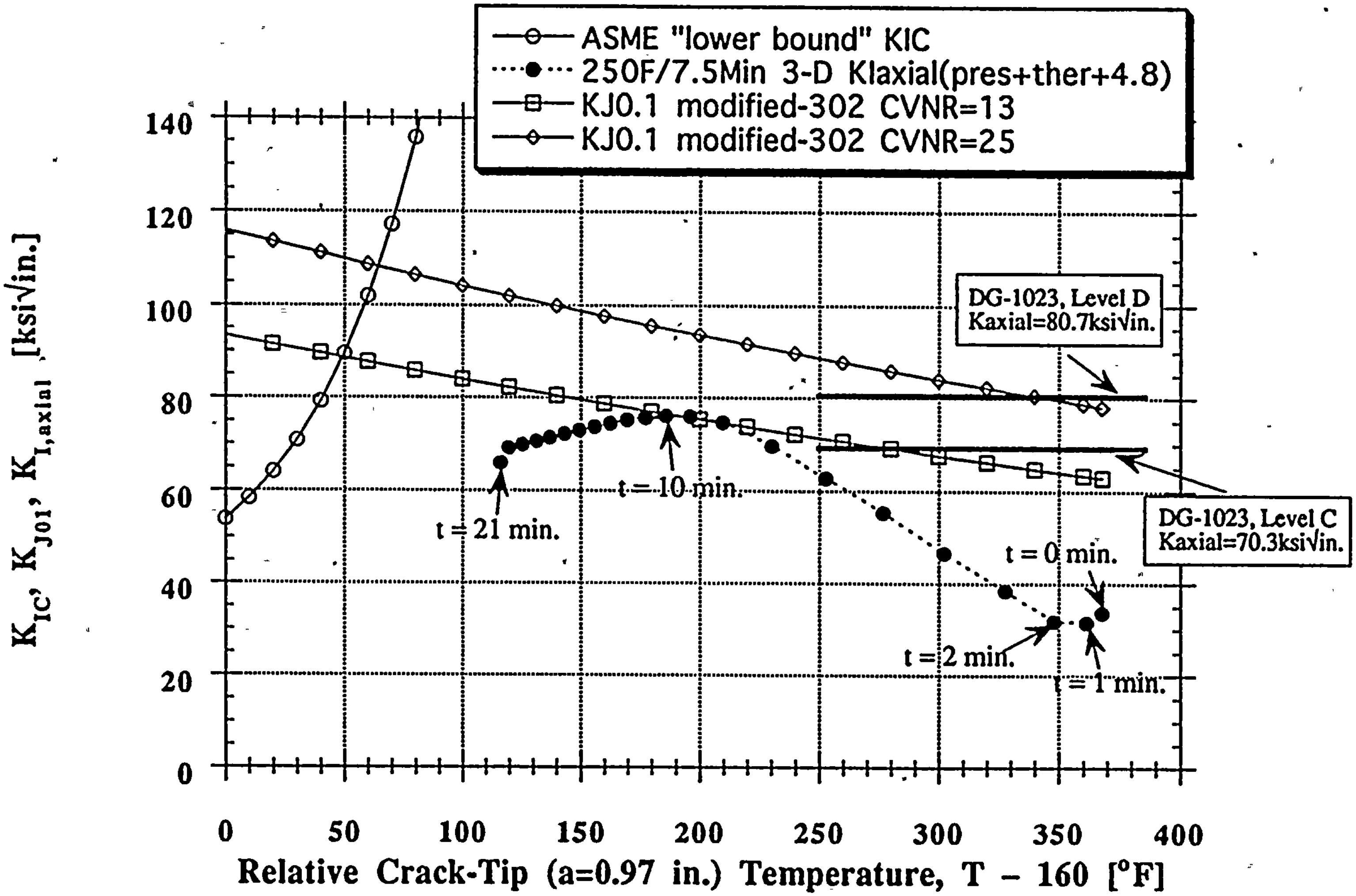
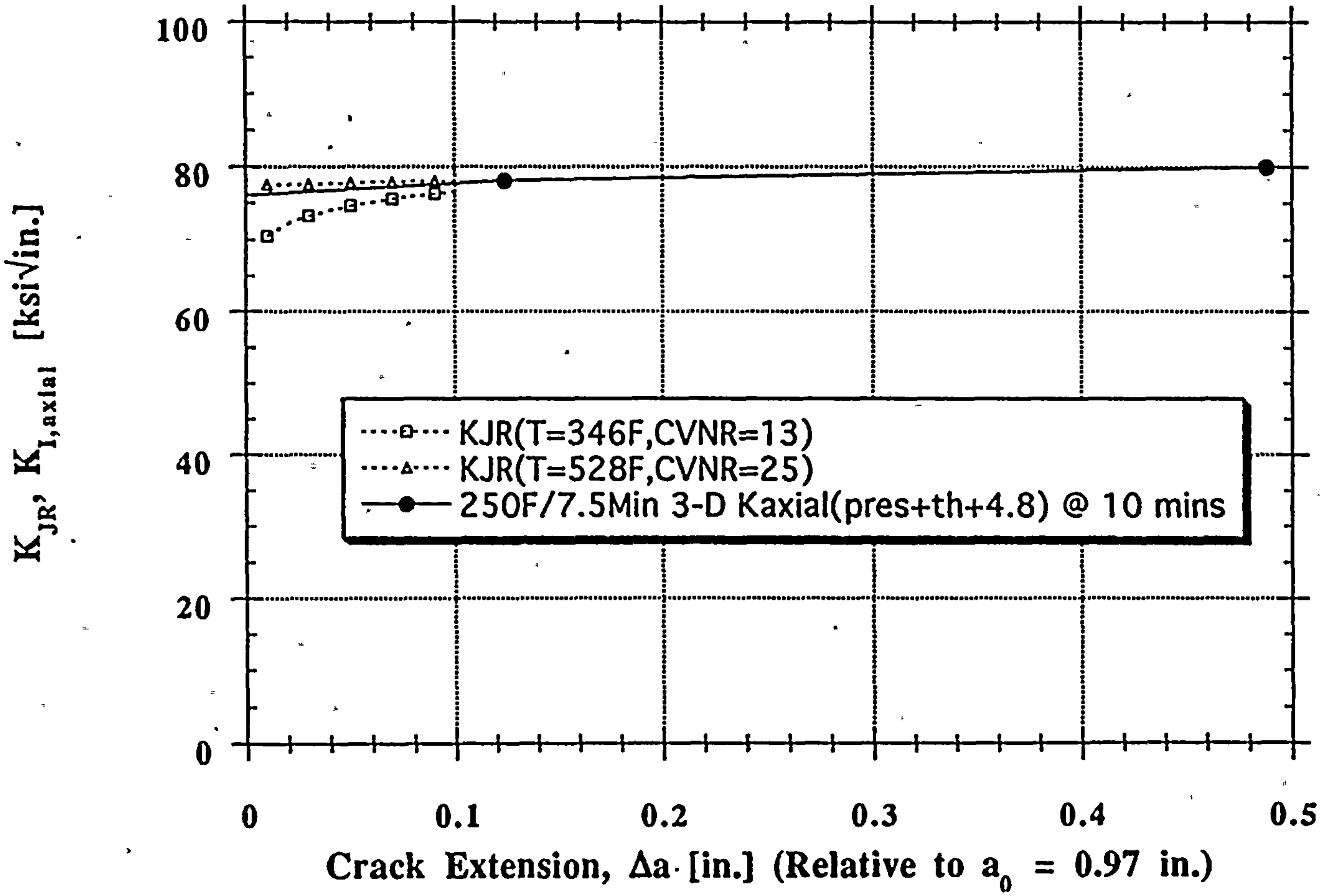

ASME "lower bound" KIC
250F/7.5Min 3-D KIaxial(pres+ther+4.8)
KJ0.1 modified-302 CVNR=13
KJ0.1 modified-302 CVNR=25
DG-1023, Level D
Kaxial=80.7ksi√in.
DG-1023, Level C
Kaxial=70.3ksi√in.
t = 21 min.
t = 10 min.
t = 0 min.
t = 2 min.
t = 1 min.
0
20
40
60
80
100
120
140
0
50
100
150
200
250
300
350
400
K_{IC}, K_{J01}, $K_{I,axial}$ [ksi√in.]
Relative Crack-Tip (a=0.97 in.) Temperature, T – 160 [°F]

Fig. 19



100
80
60
40
20
0
K_{JR}, $K_{I,axial}$ [ksi√in.]
KJR(T=346F,CVNR=13)
KJR(T=528F,CVNR=25)
250F/7.5Min 3-D Kaxial(pres+th+4.8) @ 10 mins
0
0.1
0.2
0.3
0.4
0.5
Crack Extension, Δa [in.] (Relative to a_0 = 0.97 in.)

Fig. 20

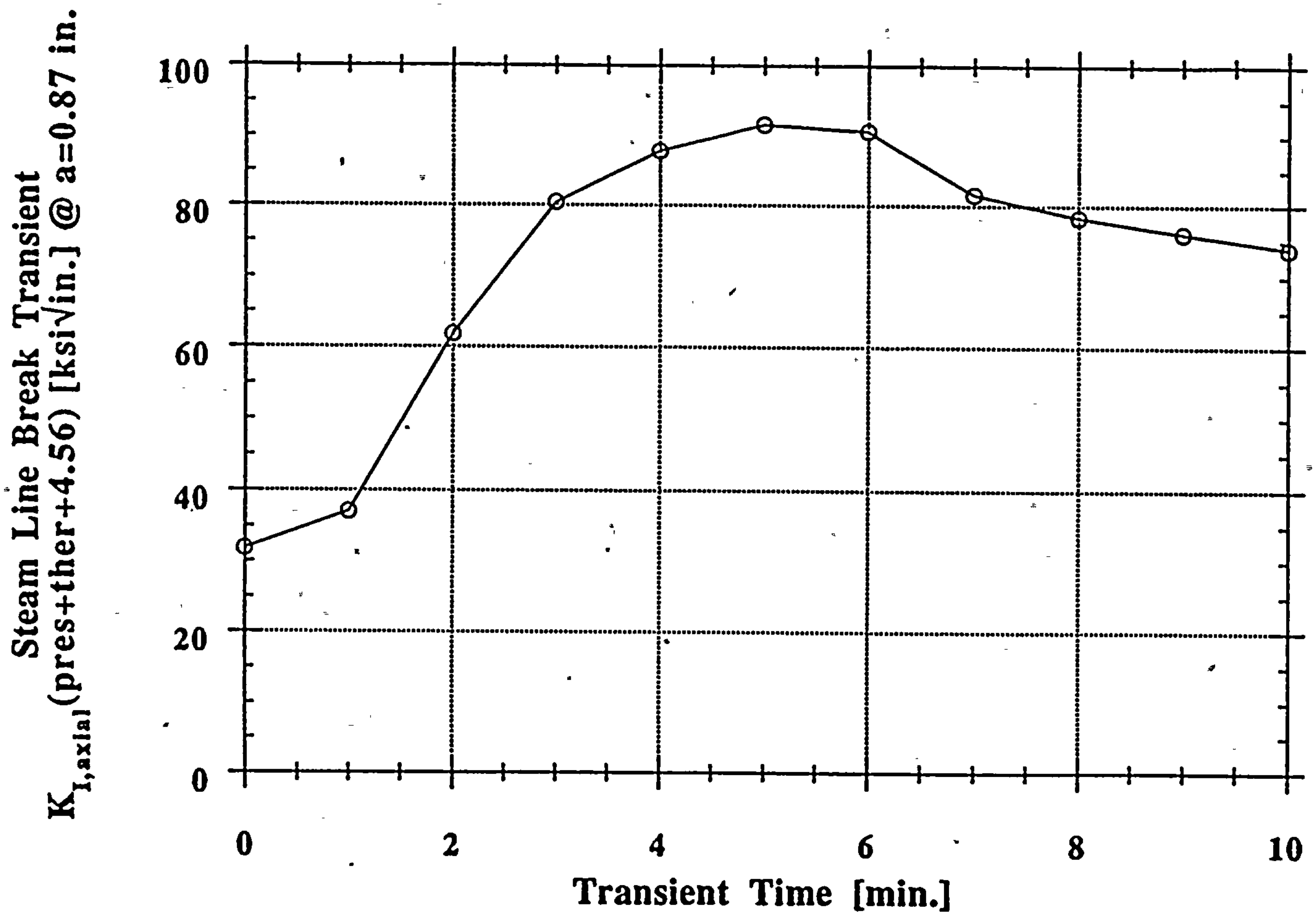

Steam Line Break Transient
$K_{I,axial}$(pres+ther+4.56) [ksi√in.] @ a=0.87 in.
100
80
60
40
20
0
0
2
4
6
8
10
Transient Time [min.]

Fig. 21

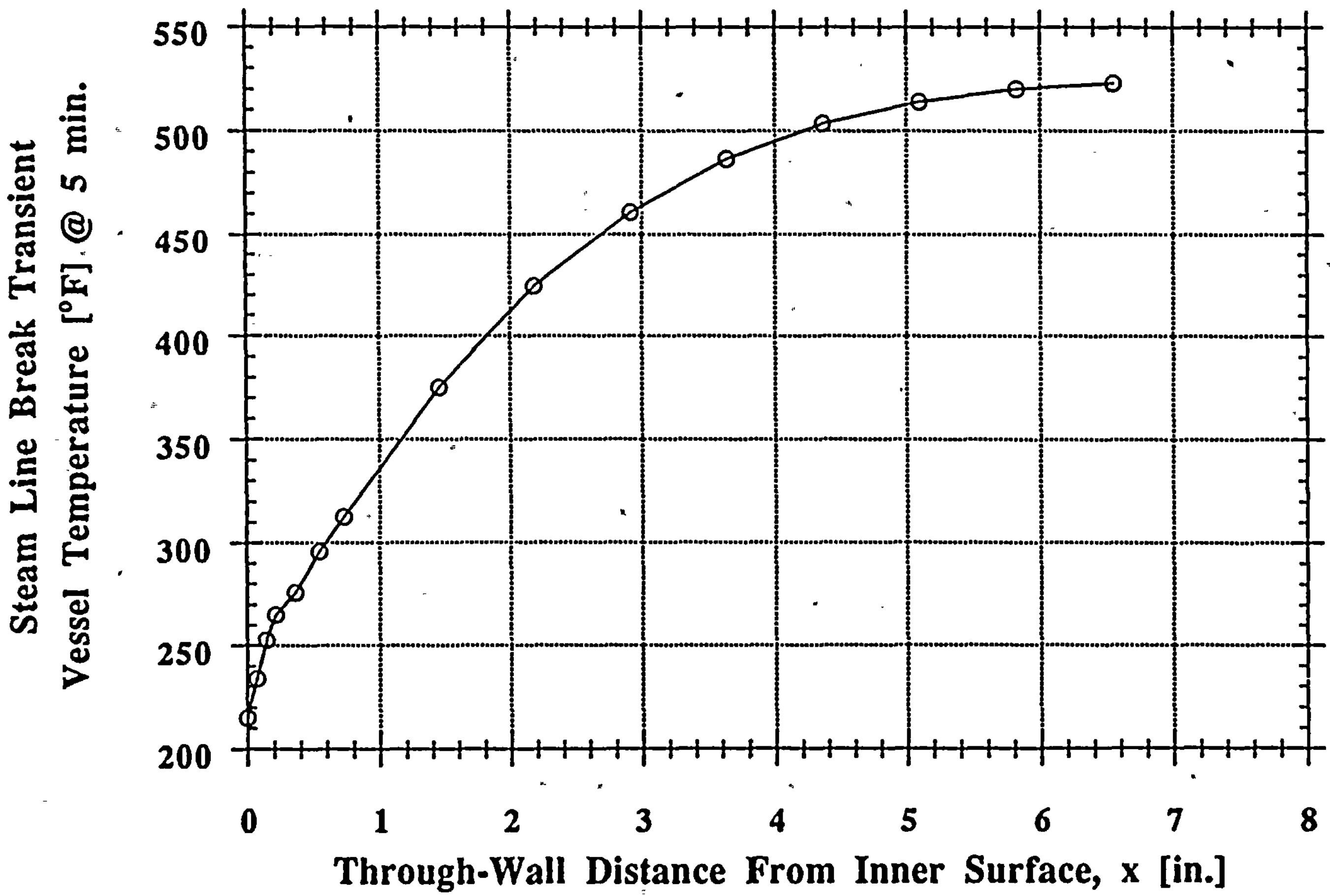
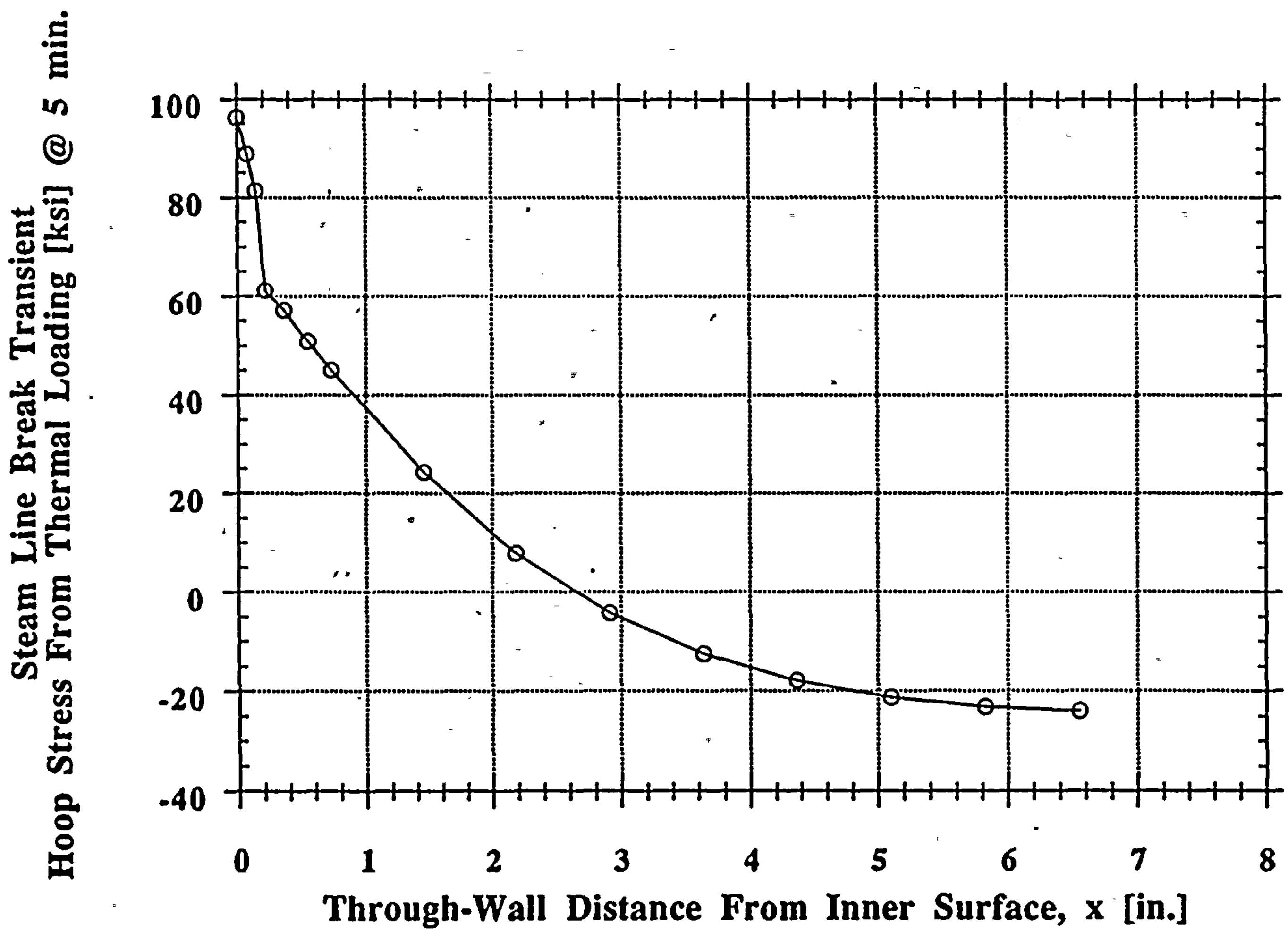
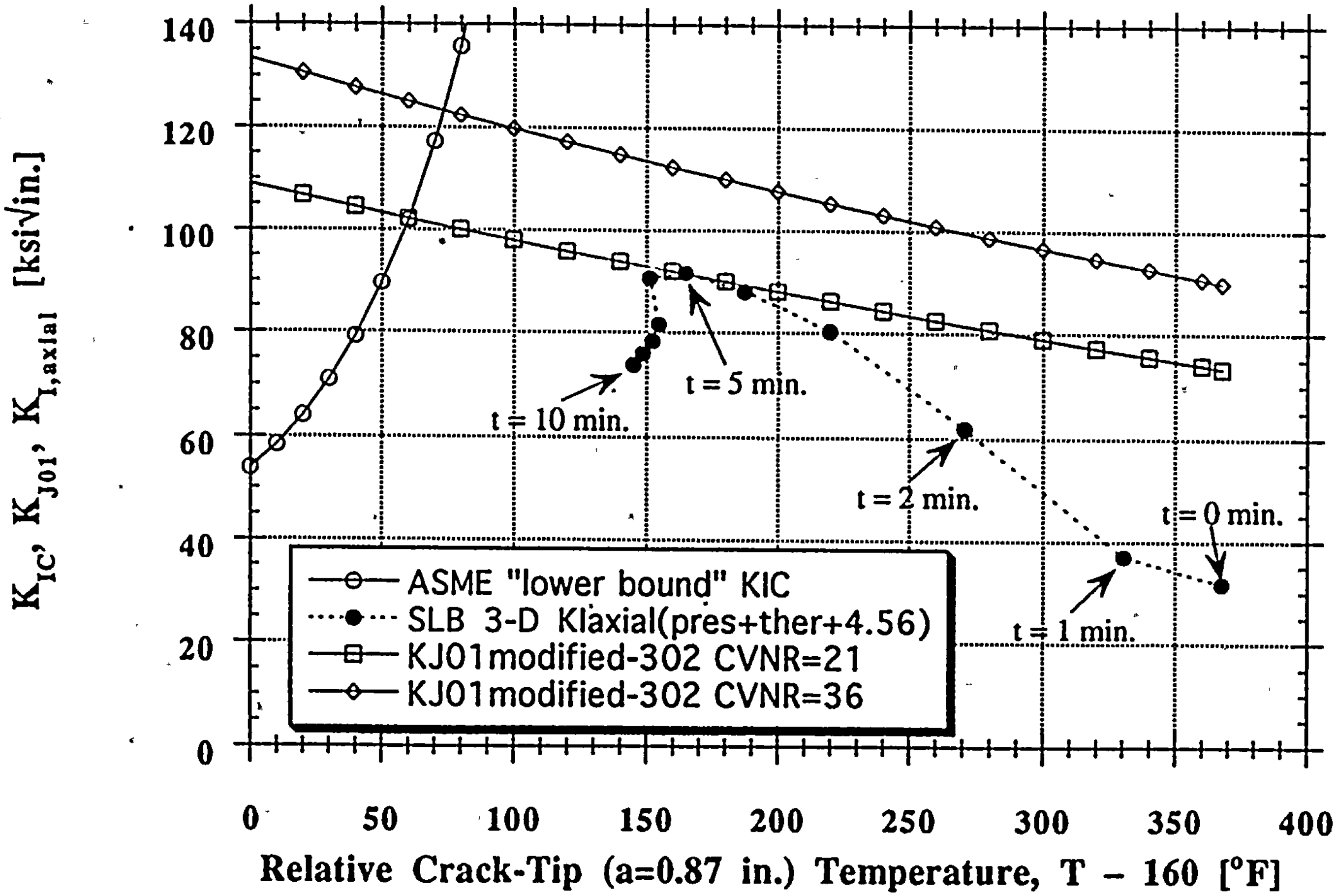

Steam Line Break Transient
Vessel Temperature [°F] @ 5 min.
550
500
450
400
350
300
250
200
0
1
2
3
4
5
6
7
8
Through-Wall Distance From Inner Surface, x [in.]

Fig. 22



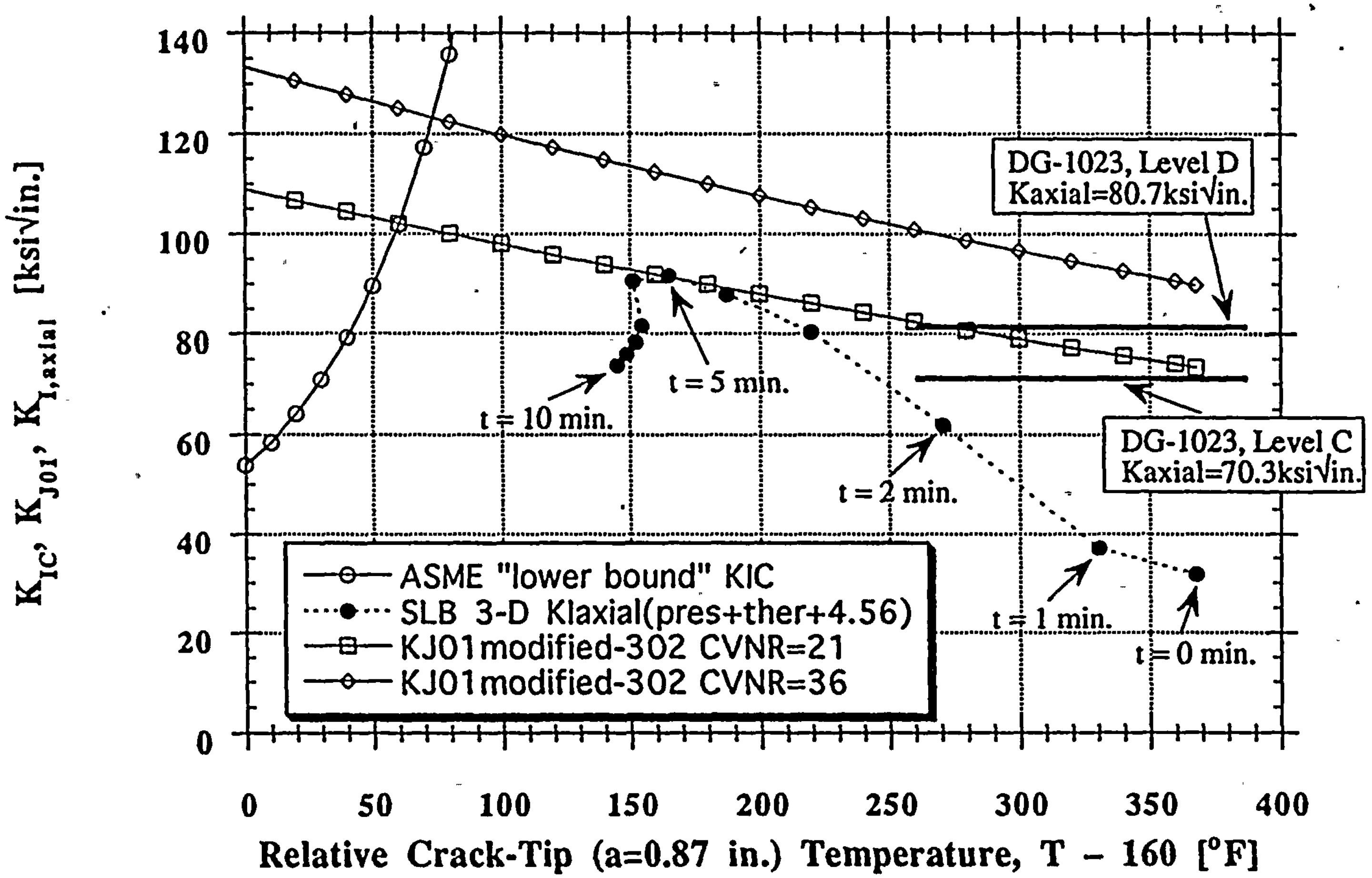
Steam Line Break Transient
Hoop Stress From Thermal Loading [ksi] @ 5 min.
100
80
60
40
20
0
-20
-40
0
1
2
3
4
5
6
7
8
Through-Wall Distance From Inner Surface, x [in.]

Fig. 23



140
120
100
80
60
40
20
0
0
50
100
150
200
250
300
350
400
Relative Crack-Tip (a=0.87 in.) Temperature, T - 160 [°F]
t = 10 min.
t = 5 min.
t = 2 min.
t = 0 min.
t = 1 min.
ASME "lower bound" KIC
SLB 3-D KIaxial(pres+ther+4.56)
KJ01modified-302 CVNR=21
KJ01modified-302 CVNR=36

Fig. 24



140
120
100
80
60
40
20
0
0
50
100
150
200
250
300
350
400
K_{IC}, K_{J01}, $K_{I,axial}$ [ksi√in.]
Relative Crack-Tip (a=0.87 in.) Temperature, T − 160 [°F]
DG-1023, Level D
Kaxial=80.7ksi√in.
DG-1023, Level C
Kaxial=70.3ksi√in.
t = 10 min.
t = 5 min.
t = 2 min.
t = 1 min.
t = 0 min.
ASME "lower bound" KIC
SLB 3-D KIaxial(pres+ther+4.56)
KJ01modified-302 CVNR=21
KJ01modified-302 CVNR=36

Fig. 25

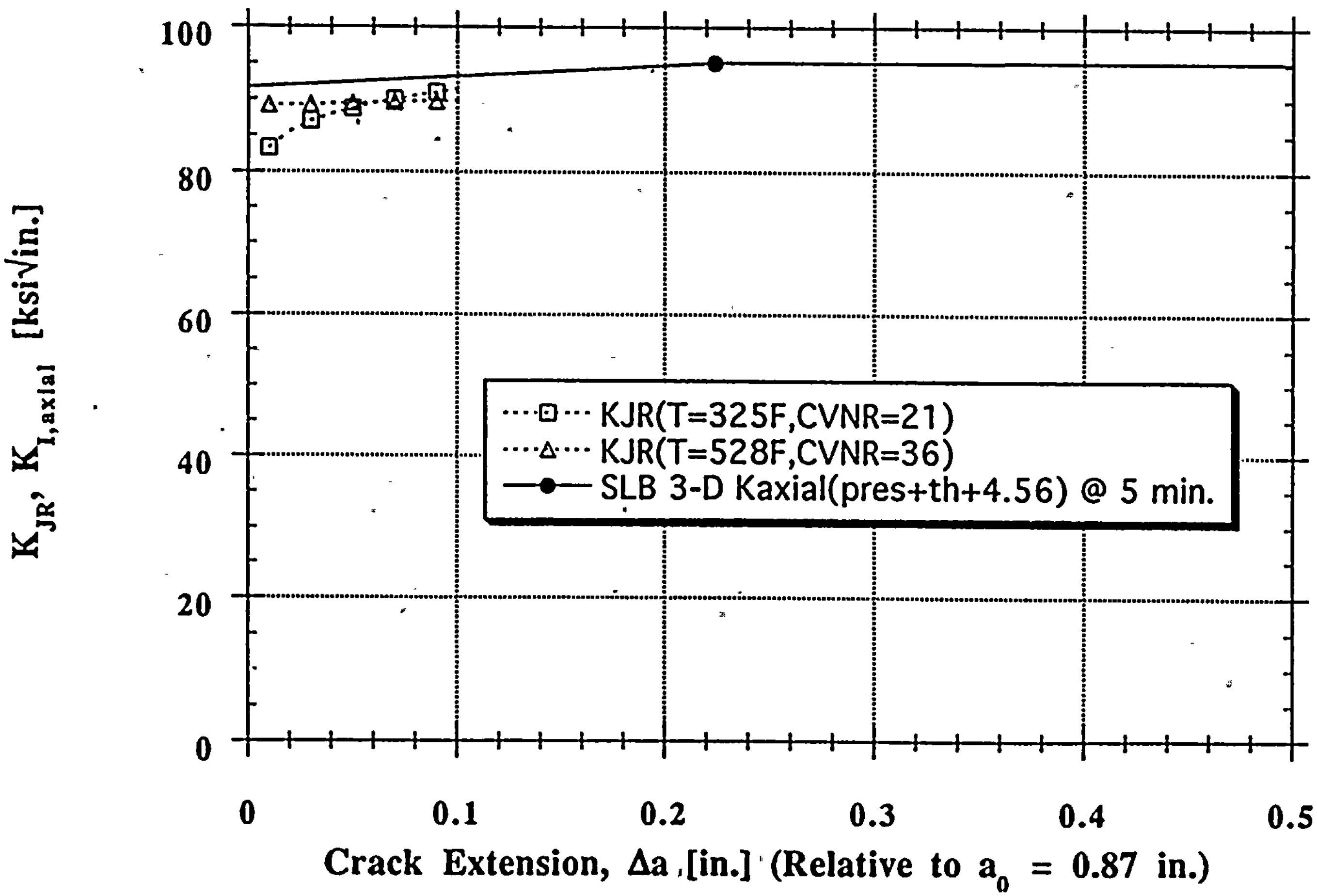

100
80
60
40
20
0
0
0.1
0.2
0.3
0.4
0.5
KJR(T=325F,CVNR=21)
KJR(T=528F,CVNR=36)
SLB 3-D Kaxial(pres+th+4.56) @ 5 min.
Crack Extension, Δa ,[in.] (Relative to a0 = 0.87 in.)

Fig. 26

This completes our effort on TAC No. M86107. Our review of NMPC's response to Generic Letter 92-01, Revision 1, "Reactor Vessel Structural Integrity" was performed under TAC No. M83486. The results of that review are being reported separately to you. Please contact the NRC NMP-1 Project Manager, Donald S. Brinkman at (301) 504-1409 if you have any questions.

Sincerely,

Original signed by:

Robert A. Capra, Director
Project Directorate I-1
Division of Reactor Projects - I/II
Office of Nuclear Reactor Regulation

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2. Technical Evaluation Report

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