

**THE METHOD OF LIFE EXTENSION
FOR THE HIGH FLUX ISOTOPE REACTOR VESSEL**

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THE METHOD OF LIFE EXTENSION FOR THE HIGH FLUX ISOTOPE REACTOR VESSEL

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ABSTRACT

The state of the vessel steel embrittlement as a result of neutron irradiation can be measured by its increase in the nil ductility temperature (NDT). This temperature is sometimes referred to as the brittle-ductile transition temperature (DBT) for fracture. The life extension of the High Flux Isotope Reactor (HFIR) vessel is calculated by using the method of fracture mechanics. A hydrostatic pressure test (hydrotest) is performed in order to determine a safe vessel static pressure. It is then followed by using fracture mechanics to project the reactor life from the safe hydrostatic pressure. The life extension calculation provides the following information on the remaining life of the reactor as a function of the nil ductility temperature increase: (1) the probability of vessel fracture due to hydrotest vs vessel life at several hydrotest pressures, (2) the hydrotest time interval vs the uncertainty of the nil ductility temperature increase rate, and (3) the hydrotest pressure vs the uncertainty of the nil ductility temperature increase rate. It is understood that the use of a complete range of uncertainties of the nil ductility temperature increase is equivalent to the entire range of radiation damage that can be experienced by the vessel steel. From the numerical values for the probabilities of the vessel fracture as a result of hydrotest, it is estimated that the reactor vessel life can be extended up to 50 EFPY (100 MW) with the minimum vessel operating temperature equal to 85°F.

1.0 INTRODUCTION

The present paper is related to the evaluation on the remaining life of the High Flux Isotope Reactor (HFIR) vessel after it has been exposed to neutron irradiation for many years of service. The state of the vessel steel embrittlement as a result of neutron irradiation can be measured by its increase in the nil ductility temperature (NDT). The fracture toughness versus temperature curve is shifted in the temperature coordinate to an amount equal to the NDT. The temperature NDT is sometimes referred to as the brittle-ductile transition temperature (DBT) for fracture. For temperatures less than the DBT, the body-centered metals will fracture in brittle fracture mode and it will fracture in ductile fracture mode for temperatures greater than the DBT. Steel subjected to neutron irradiation will shift the DBT to a higher temperature, thereby exposed to a wider range of brittle fracture region. In contrast to brittle fracture, the ductile fracture mode involves active dislocation emission mechanism at the crack tip during the fracture process. Therefore, the DBT is the temperature at which the dislocation emission mechanism at the crack tip begins to become active.

The remaining life of HFIR is calculated by using the method of fracture mechanics with the toughness data of the irradiated reactor vessel steel. The method is incorporated with a hydrostatic pressure test (hydrotest). The test is performed to determine the safe hydrostatic pressure of the vessel. The projection of the remaining life of the reactor vessel is made by using the method of fracture mechanics from the safe hydrostatic pressure obtained from the hydrotest. Naturally, the

probability of fracture as a result of hydrotest should be a fairly small value. Here, an upper bound is assumed to be the reactor core melt probability of 10^{-4} . This probability of fracture value is used to set an upper limit beyond which this method of life extension will not be applicable. Therefore, this upper limit may be used to determine the total life of the reactor provided that there is no other applicable method of life extension.

The method of life extension analysis for the High Flux Isotope Reactor is mainly designed by Cheverton (ref. 1). Recently, he made a life extension calculation (refs. 2 and 3). The present paper is intended to confirm his analysis. His probability of fracture calculation for the reactor vessel, because of the hydrotest, was carried out by applying the Monte Carlo simulation. An alternative approach is used in this paper to obtain his probability of fracture result regarding the reactor vessel fracture as a result of the hydrotest. This method is very simple. Yet, it is capable to produce essentially the same probability of fracture value. This method of probability of fracture calculation has made use of the Marshall crack density distribution function that provides an estimate on the fraction of cracks that exceeds the critical crack length. This method was used earlier for the HFIR vessel severe accident analysis (ref. 4). Furthermore, in this paper the numerical results for the hydrotest interval are calculated for a complete range of radiation damage. Therefore, it is possible to observe the complete results from this life extension method. The limiting value for the maximum radiation damage is also derived. The calculation of the hydrotest time interval reveals that for small values of radiation some part of the curve may have negative values. The limitation that the hydrotest time interval remains to be positive determines the range of valid hydrotest temperature and pressure.

2.0 METHOD OF ANALYSIS

The method of fracture mechanics is applied to estimate the remaining life of the HFIR vessel. To apply fracture mechanics, it is required to make a good estimate of : (1) the location and dimension of a critical crack, (2) the fracture toughnesses of the reactor vessel steel at different levels of neutron irradiation, and (3) the pressure and temperature loading condition. The above conditions can be prescribed either by a deterministic estimate or by a probabilistic description. Here, the crack size is expressed by the Marshall distribution and the toughness is represented by the measured nil ductility temperature of the steel that is multiplied by a factor of uncertainty.

The hydrostatic pressure test is used to determine a safe vessel pressure and temperature. From the safe hydrotest pressure and temperature, the vessel safe operating conditions is projected for the subsequent years of operation. Naturally, the reactor vessel has to be first analyzed and verified that the vessel has a very small probability of fracture as a result of hydrotest. Therefore, the hydrotest will not be a viable method once the fracture probability, because of the hydrotest, exceeds certain prescribed value.

To calculate this probability of fracture, the Marshall distribution is used. The Marshall distribution function of crack size and population is assumed to be independent of the number of hydrotests that have been performed and also to be independent of the degree of embrittlement experienced by the vessel. The crack density of 0.007 cracks/ft² near beam tube is assumed (ref. 1). In the earlier paper (ref. 4), the probability of a crack that exceeds the crack depth a is derived from the Marshall distribution as

$$P(a) = \frac{0.005}{0.59} \exp(-4.1a) + \frac{0.585}{0.59} \exp(-6.97a) \quad (2.1)$$

The probability of fracture because of hydrotest can be obtained from the above equation if a is the critical crack length for fracture. The critical crack length a can be obtained from the assumed crack configuration, the applied load and the vessel steel embrittlement condition.

For the purpose of projecting future operational safety from the hydrotest, a criterion is proposed by Cheverton that the potential for vessel fracture during the hydrotest is equal to or greater than the potential of vessel fracture at the time between the two successive hydrotests with the vessel subjected to worst-case loading condition of pressure and temperature (ref. 1). The criterion is

$$\frac{K_I(HT)}{K_{IC}(HT)} \geq \frac{K_I(SV)}{K_{IC}(\Delta t)} \quad (2.2)$$

where

$K_I(HT)$ = stress intensity factor at the time of hydrotest,
 $K_I(SV)$ = stress intensity factor corresponding to worst-case loading conditions,
 $K_{IC}(HT)$ = fracture toughness at the time of hydrotest, and
 $K_{IC}(\Delta t)$ = minimum fracture toughness at the time interval of the two successive hydrotests and corresponding to worst-case loading conditions.

The HFIR vessel embrittlement rate is based on test results (refs. 1 and 2). For a corner crack of 1.0 in. deep, the rate of RT_{NDT} increase is 2.44 °F/ EFPY (100 MW). A vertical corner crack of 1.0 in. deep in the nozzle weld and located directly above the center of beam tube HB3 nozzle is considered most critical in the fracture analysis. This crack configuration is used to analyze hydro-time interval and hydro pressure vs the uncertainty e of the RT_{NDT} increase rate. It is assumed that there is no credible crack growth mechanism for the HFIR structure either under regular operating condition or under hydrotest condition (refs. 2 and 3).

3.0 CALCULATION AND ANALYSIS

3.1 Stress intensity factor as a function of system pressure is

$$K_I = (p + S) \cdot G \sqrt{\pi a} \quad (3.1)$$

where

a = crack depth
 p = primary system pressure
 G = geometric factor for the corner crack due to primary system pressure,
 S = contribution to the stress intensity factor from the residual stress, expressed as the equivalent primary system pressure.

Probabilities of fracture as a result of hydrotest are plotted in Fig. 1 for several hydrotest pressures.

3.2 Fracture toughnesses

A number of fracture toughnesses are defined here, because toughnesses at different stages of material embrittlement are used in the calculation.

For an irradiated steel, the toughness is modeled by

$$K_{IC} = A + B \exp [C (T - RT_{NDT})] \quad (3.2)$$

where

$$\begin{aligned} T &= \text{material temperature,} \\ RT_{NDT} &= RT_{NDT0} + \Delta RT_{NDT} \\ RT_{NDT0} &= \text{initial (undamaged) value of the nil ductility temperature,} \\ \Delta RT_{NDT} &= \text{increase in } RT_{NDT} \text{ due to radiation,} \\ A, B, C &= \text{constants.} \end{aligned}$$

The fracture toughness at hydrotest is

$$K_{IC}(HT) = A + B \exp [C \cdot E(HT)] \quad (3.3)$$

and the fracture toughness between two hydrotests is

$$K_{IC}(\Delta t) = A + B \exp [C [E(\Delta t) - e \cdot RRT_{NDT} \cdot \Delta t]] \quad (3.4)$$

The E-function is used to describe an accumulation of the different stages of the NDT temperature increases,

$$E(HT) = T_v(HT) - RT_{NDT0} - e \cdot RRT_{NDT} \cdot \Delta t(HT) \quad (3.5)$$

and

$$E(\Delta t) = T_v(\Delta t) - RT_{NDT0} - e \cdot RRT_{NDT} \cdot \Delta t(HT) \quad (3.6)$$

In the above equations,

$$\begin{aligned} RRT_{NDT} &= \text{radiation time rate of increase in } RT_{NDT}, \\ e &= \text{uncertainty factor associated with the rate } RRT_{NDT}, \\ \Delta t &= \text{time between hydrotests,} \\ \Delta t(HT) &= \text{total operating time up to time of hydrotest,} \\ T_v(HT) &= \text{temperature of vessel during hydrotest,} \\ T_v(\Delta t) &= \text{lowest (worst) operating temperature of vessel at any time during } \Delta t. \end{aligned}$$

3.3 Time interval between hydrotests as a function of e is (refs. 2 and 3)

$$\Delta t = \frac{E(\Delta t) - \frac{1}{C} \ln \left[\frac{A}{B} \frac{K_{IC}(HT)}{A} \left(\frac{p(SV) + S}{p(HT) + S} \right) - 1 \right]}{e \cdot RRT_{NDT}} \quad (3.7)$$

where

$$\begin{aligned} p(HT) &= \text{hydrotest pressure,} \\ p(SV) &= \text{worst possible operating pressure,} \end{aligned}$$

Numerical values of the hydrotest time interval are shown in Fig. 2 and Fig. 3. The hydro time interval Δt for lower operation temperatures turns to negative value if the uncertainty e is extended to smaller values. This implies that at low operation temperatures ($\leq 70^\circ\text{F}$), the hydro test will not be valid. For each Δt , there exists a region of valid hydro P versus T . The boundary curves of these regions will be plotted and issued later. The operating temperatures considered in the report are higher than 70°F because operation temperatures below 70°F were beyond the region of interest.

3.4 Pressure of hydrotest as a function of e is (refs. 2 and 3)

$$p(HT) = \frac{K_{IC}(HT)}{K_{IC}(\Delta t)} p(SV) + S \left[\frac{K_{IC}(HT)}{K_{IC}(\Delta t)} - 1 \right] \quad (3.8)$$

The hydrotest pressure plots are shown in either Figs. 4 and 5 for a complete range of the uncertainty e . It is observed that the curves approach a unique value of pressure $p(HT)$ as e tends to infinity. The reason is that all the toughnesses tend to a unique value at fully damaged state.

3.5 The plots for the hydrotest time interval Δt goes to infinity at either $e = 0$ or at some large e value. Analytically, this critical value can be determined easily by using the expression of Δt from Equation 3.7. The logarithmic value goes to minus infinity as its argument approaches zero. The critical e value that gives very large hydrotest interval is the solution of:

$$E(HT) = \frac{1}{C} \ln \left[\frac{A}{B} \left[\frac{p(HT) + S}{p(SV) + S} - 1 \right] \right] \quad (3.9)$$

Therefore, from Equation 3.5,

$$e = \frac{T_{\Delta}(HT) - RT_{NDT} - \frac{1}{C} \ln \left[\frac{A}{B} \left[\frac{p(HT) + S}{p(SV) + S} - 1 \right] \right]}{RRT_{NDT} \cdot \Delta t(HT)} \quad (3.10)$$

The hydrotest time interval Δt at $e = 0$ is either $+\infty$ or $-\infty$, except that the numerator of Equation 3.7 is also zero. For this case, there is a finite Δt value not necessarily equal to zero. For $T_{\Delta}(HT)$ below that value, Δt tends to minus infinity as e approaches zero. Negative Δt is physically unrealistic.

4.0 RESULTS AND CONCLUSION

The probability of failure of HFIR vessel at hydrotest pressure of 900 psi with a 50 EFPY (100 MW) life extension is less than 10^{-4} , that is the reactor core melt probability. The minimum hydrotest interval based on the hydro pressure of 900 psi is approximately 10 EFPY (100 MW). A reduction of operating temperature from 85 to 80°F results in a reduction of hydrotest time interval from approximately 10 EFPY (100 MW) to 6.5 EFPY (100 MW).

5.0 REFERENCES

1. Cheverton, R.D., Merkle, J. G. and Nanstad, R. K., 1988, "Evaluation of HFIR Pressure Vessel Integrity Considering Radiation Embrittlement," ORNL/TM-10444.
2. Cheverton, R.D. and Nanstad, R.K., 1994, "Evaluation of HFIR Vessel Surveillance Data and Hydrotest Conditions," ORNL/TM-12758.
3. Cheverton, R.D., 1994, "An Evaluation of Life Extension of the HFIR Pressure Vessel," ORNL/TM/12877, 1994.
4. Chang, S.J., 1994, "Probability of Fracture for HFIR Pressure Vessel Caused by Random Crack Size or by Random Toughness," Transactions of ASME, J. of Pressure Vessel Technology, Vol. 116, pp. 24-29.

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FIGURE CAPTIONS

1. The hydro test conditional probability of fracture $P(F/E)$ vs the operating time at hydro test for hydro temperature $T_v(HT) = 85^\circ\text{F}$ and for several hydro pressures $p(HT)$. The probability curves are obtained by using the Marshall distribution.
2. Hydro time interval Δt vs the uncertainty e of the nil ductility temperature increase rate. The plots are based on HFIR vessel embrittlement age $\Delta t(HT) = 19.4$ EFPY (100 MW) and a range of operating temperatures $T_v(\Delta t)$. The worst-case operating pressure $p(SV)$ is 679 psi. The hydro pressure $p(HT)$ is 900 psi and ASME lower bound fracture toughness is assumed.
3. Hydro time interval Δt vs the uncertainty e of the nil ductility temperature increase rate. The plots are based on HFIR vessel embrittlement age $\Delta t(HT) = 19.4$ EFPY (100 MW) and a range of operating temperatures $T_v(\Delta t)$. The worst-case operating pressure $p(SV)$ is 679 psi. The hydro pressure $p(HT)$ is 900 psi and the average fracture toughness is assumed.
4. Hydro pressure $p(HT)$ vs the uncertainty e of the nil ductility temperature increase rate. The plots are based on hydro time interval $\Delta t = 1$ EFPY, HFIR vessel embrittlement age $\Delta t(HT) = 19.4$ EFPY (100 MW), ASME lower bound fracture toughness, and a range of operating temperatures $T_v(\Delta t)$. The worst-case operating pressure $p(SV)$ is 679 psi.
5. Hydro pressure $p(HT)$ vs the uncertainty e of the nil ductility temperature increase rate. The plots are based on hydro time interval $\Delta t = 1$ EFPY, HFIR vessel embrittlement age $\Delta t(HT) = 19.4$ EFPY (100 MW), the average fracture toughness, and a range of operating temperatures $T_v(\Delta t)$. The worst-case operating pressure $p(SV)$ is 679 psi.

Probability of Fracture $Ibdk1c$

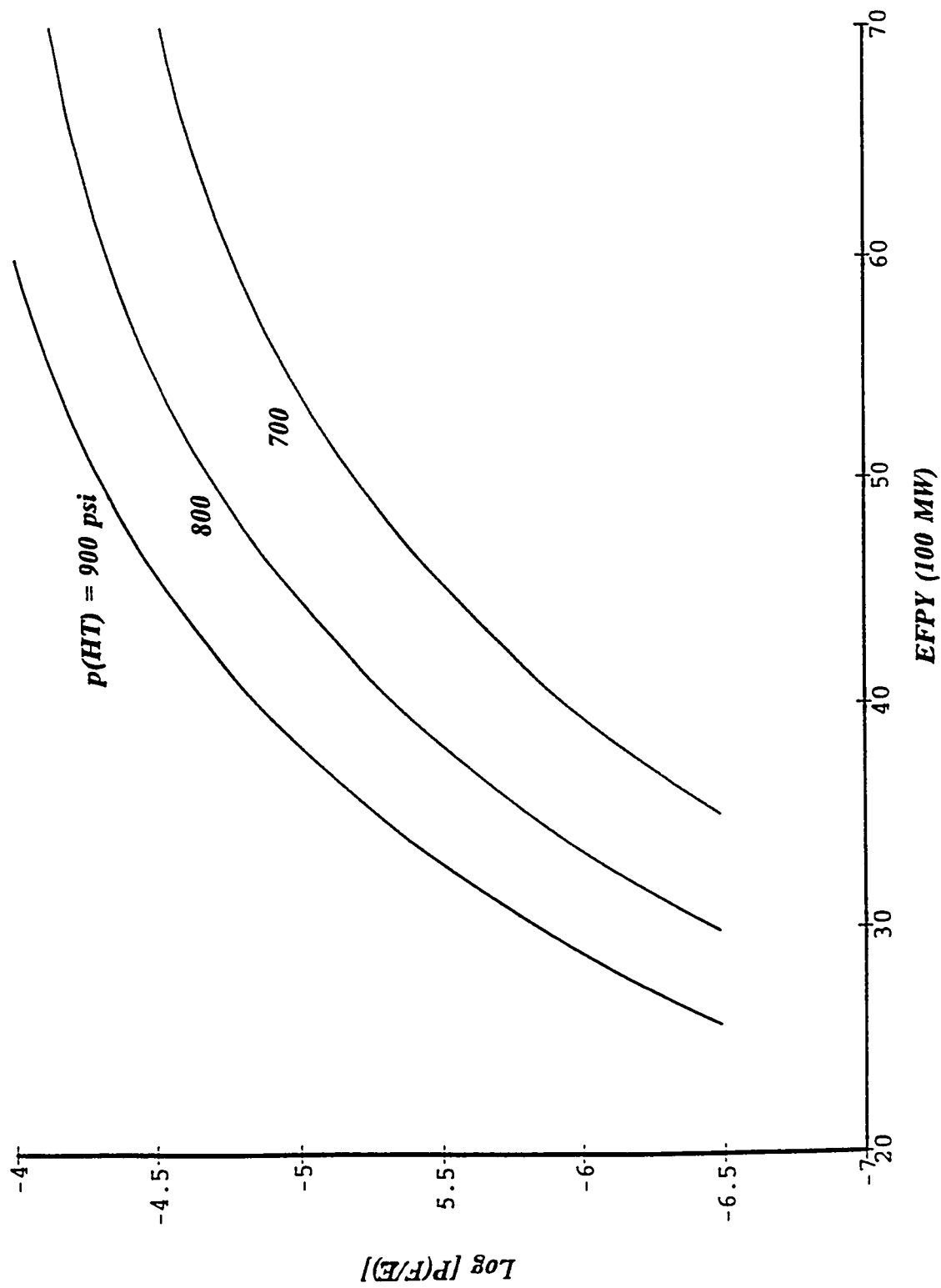
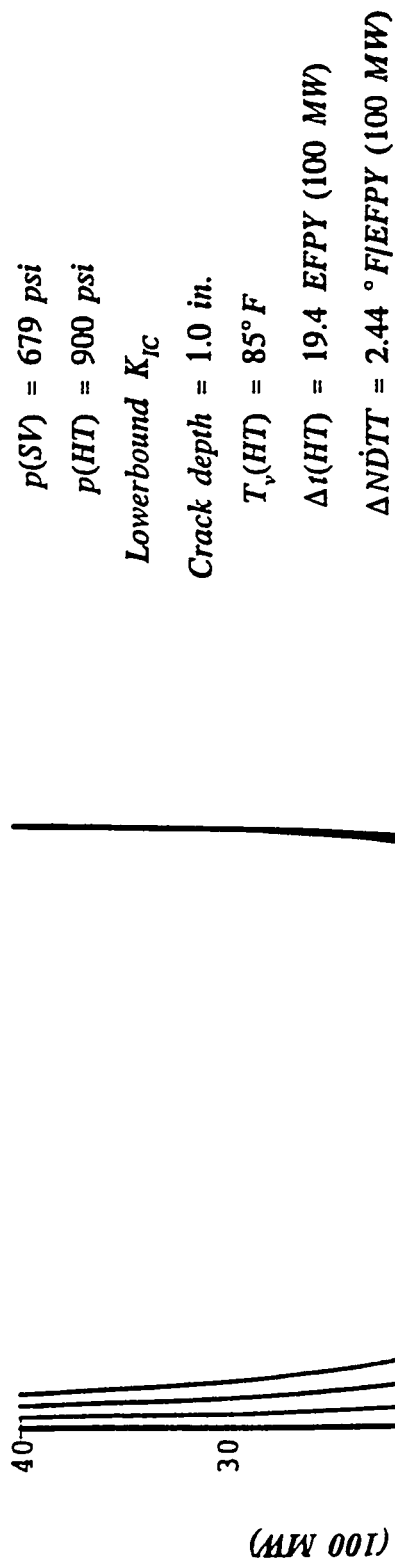


Fig. 1

86 11 (27)

Hydro Time Interval



e , Uncertainty of Nil Ductility Temperature Increase Rate

Fig. 4

10-11-28

Hydro Time Interval

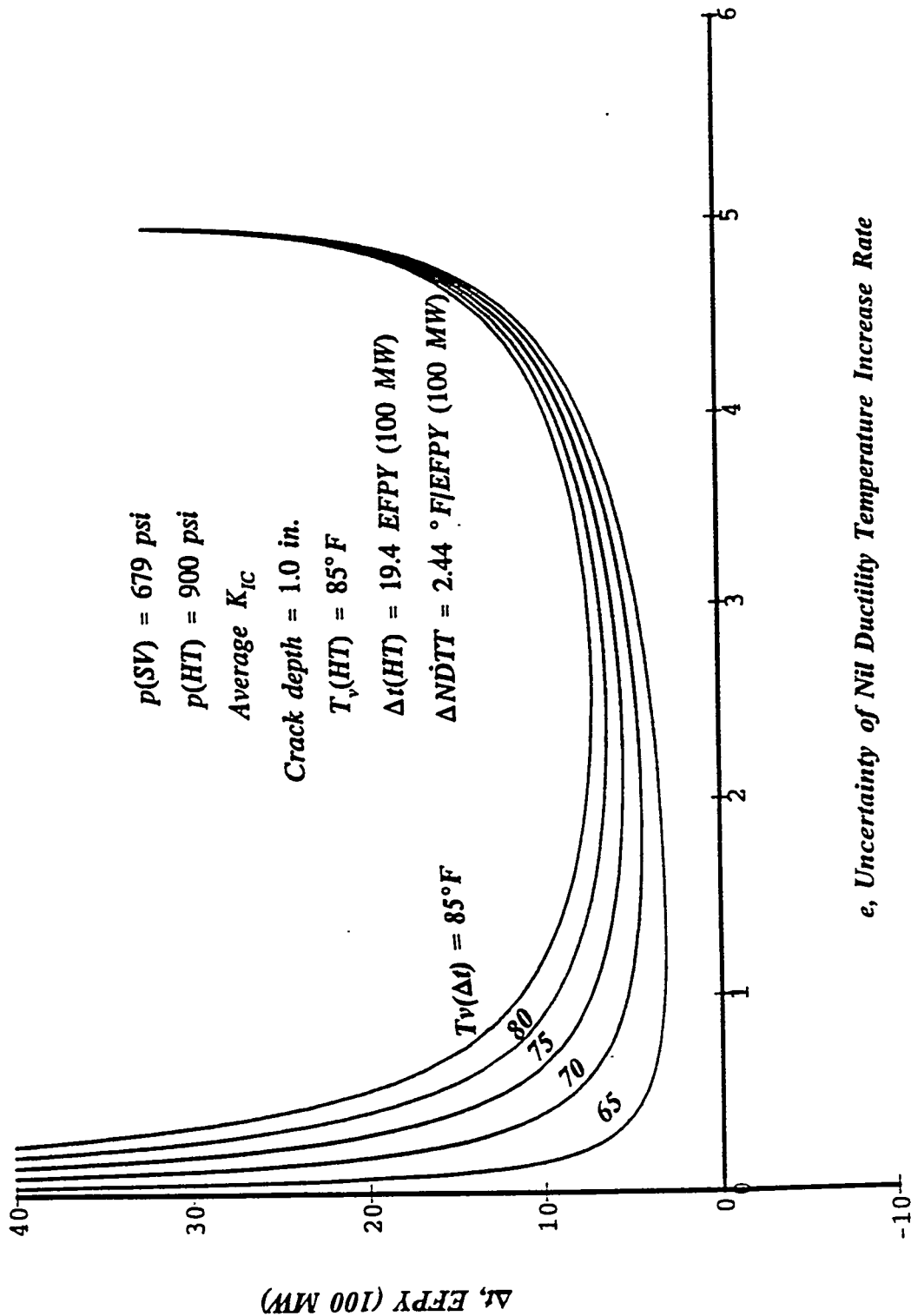
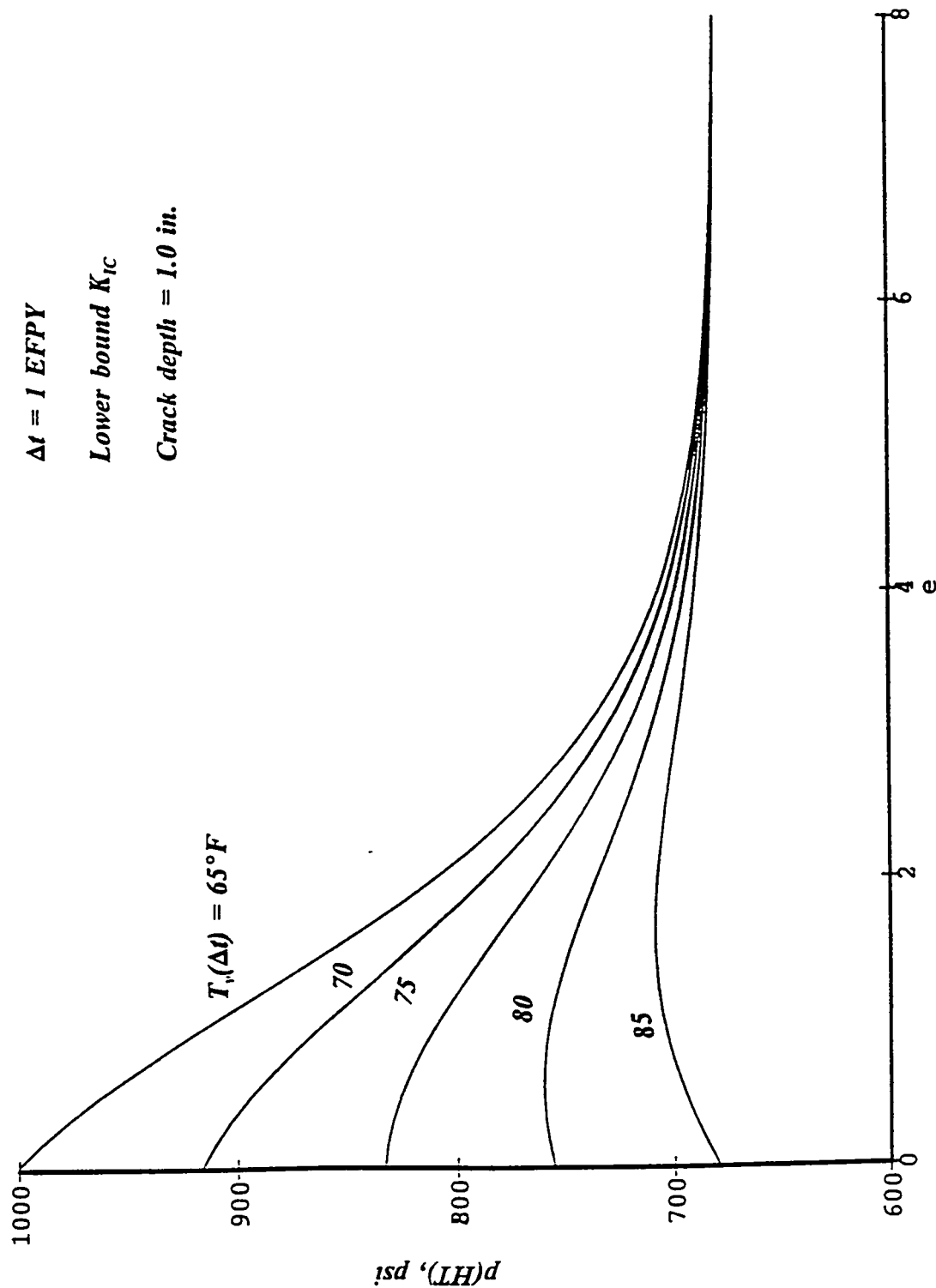


Fig. 3



e, Uncertainty of Nil Ductility Temperature Increase Rate

82 K(2.7)

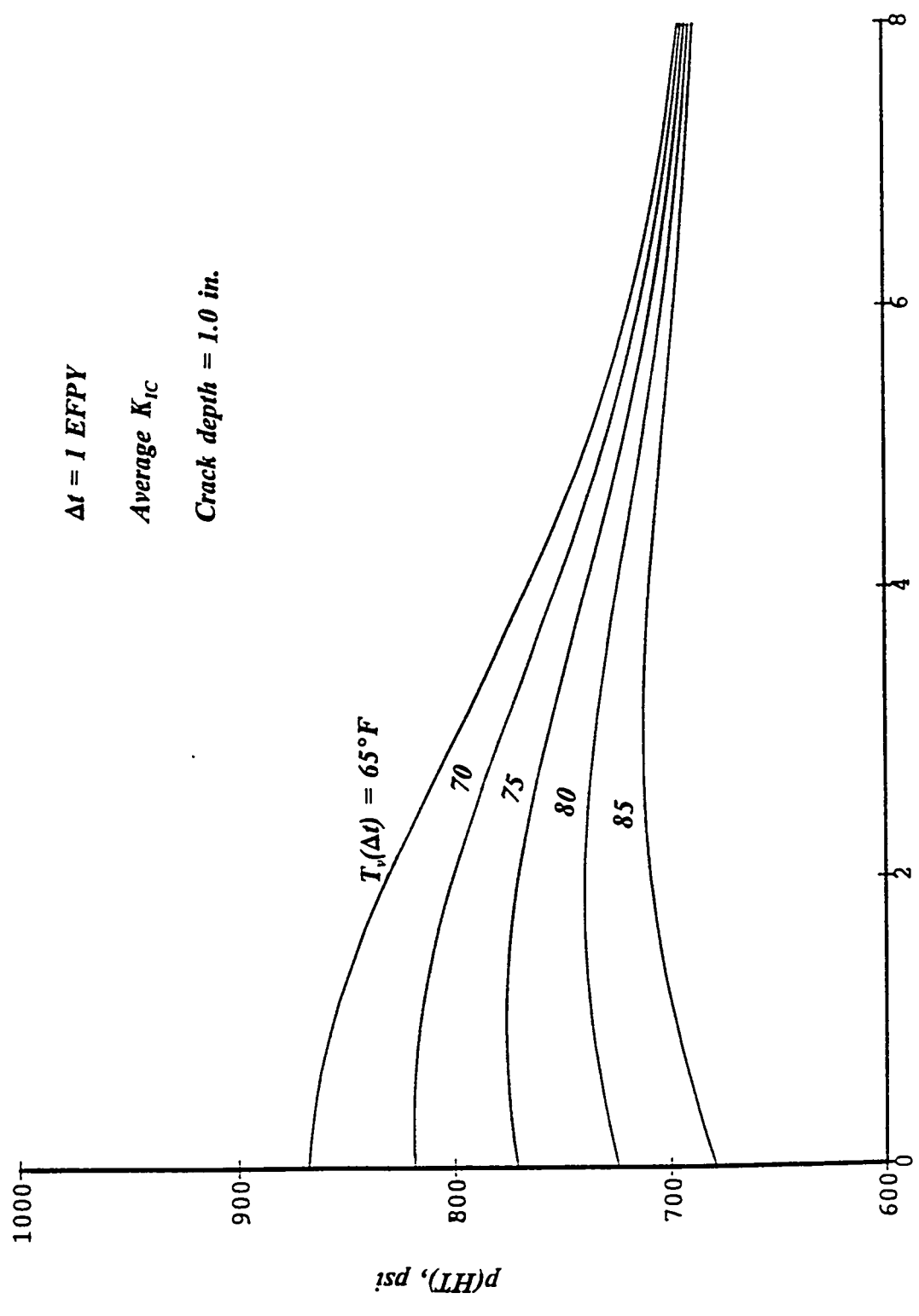
8c KC-47

Hydro Pressure

$\Delta t = 1 \text{ EFPPY}$

Average K_{IC}

Crack depth = 1.0 in.



e, Uncertainty of Nil Ductility Temperature Increase Rate