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An Evaluation of Life Extension of the HFIR Pressurizer Vessel Supplement 1

R. D. Cheverton

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**AN EVALUATION OF LIFE EXTENSION OF THE
HFIR PRESSURE VESSEL
SUPPLEMENT 1**

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ABSTRACT

Preliminary analyses were performed in 1994 to determine the remaining useful life of the HFIR pressure vessel. The estimated total permissible life was ~ 50 EFPY (100 MW). More recently, the analyses have been updated, including a more precise treatment of uncertainties in the calculation of the hydrostatic-proof-test conditions and also including the contribution of gammas to the radiation-induced reduction in fracture toughness. These and other refinements had essentially no effect on the predicted useful life of the vessel or on the specified hydrostatic proof-test conditions.

1. INTRODUCTION

The High Flux Isotope Reactor (HFIR) vessel was originally designed for 20 effective-full-power years at 100 MW [EFPY (100 MW)], and a vessel-materials surveillance program was instituted to check on the predicted radiation damage rate.¹ In November 1986, at 17.5 EFPY (100 MW), a reevaluation of vessel integrity was initiated and was completed in 1988 (Ref. 2). At the outset of this study, Oak Ridge National Laboratory (ORNL) management requested an 8.5 EFPY (100 MW) life extension beyond 17.5 EFPY (100 MW), and in response the power level was reduced from 100 MW to 85 MW to help achieve this goal.²

Radiation damage to the vessel during the 8.5 EFPY (100 MW) life extension was expected to be such that the ASME Code requirements for nuclear vessels^{3,4} could not be satisfied. Thus, hydro testing and a probabilistic evaluation were introduced by ORNL² to satisfy the intent of the ASME Code, and this was accepted by the Department of Energy (DOE).⁵

It was apparent in 1987 that the hydro-test/probabilistic-evaluation approach to demonstrating vessel integrity could extend the life of the vessel beyond $17.5 + 8.5 = 26$ EFPY (100 MW). In 1993, as a part of the continuing surveillance program, additional surveillance specimens were tested, and the method for determining the hydro-test conditions was reviewed and modified.⁶ One important result of this effort was the confirmation of the notion that the life of the vessel could be extended beyond 26 EFPY (100 MW).

By 1994, it was determined that a longer life than 26 EFPY (100 MW) was desirable, and a preliminary study addressing this issue was completed in December 1994.⁷ Results of this study indicated a total useful life of ≈ 50 EFPY (100 MW), the limitation being the probability of vessel failure during the hydro test.

Since the publication of Ref. 7, several refinements have been made in the evaluation of vessel life extension. They include an alternative approach for treating uncertainties, an evaluation and the inclusion of flaw growth (a vessel degradation mechanism), the time dependence of hydro-test conditions, and the inclusion of the effect of gamma radiation on the spatial distribution of embrittlement. The net result of the inclusion of these refinements/modifications is a small change in the recommended hydro-test conditions, a small increase in the calculated probability of vessel failure, but no change in the predicted useful life of the vessel.

2. TREATMENT OF UNCERTAINTIES

2.1 Review and Revisions

The probability of vessel failure during a hydro test is a limiting factor for vessel life extension, and the probability is a function of hydro-test pressure [p(HT)] and temperature [T(HT)]. Increasing p(HT) increases the probability of failure, but it also increases the permissible time between hydro tests (Δt), which is an operational advantage. Thus, there is a desire to reduce p(HT) and increase Δt as much as possible. This requires a close examination of uncertainties in the analysis and of related modeling features in an attempt not to be overly conservative.

The various uncertainties that must be considered can be identified by examining the equations for calculating the hydro-test conditions. The equations are included in Ref. 6 and are also included here for convenience.

A basic criterion for establishing hydro-test conditions is that the potential for vessel failure during the hydro test must be equal to or greater than the potential for "worst case" operating conditions during the time between hydro tests (Δt). This can be expressed mechanistically and mathematically as

$$\frac{K_I(HT)}{K_{Ic}(HT)} \geq \frac{K_I(E/F)}{K_{Ic}(E/F)}, \quad (1)$$

or

$$\frac{K_I(HT)}{K_I(E/F)} \geq \frac{K_{Ic}(HT)}{K_{Ic}(E/F)}, \quad (2)$$

where

$K_I(HT)$	=	stress intensity factor at time of hydro test
$K_I(E/F)$	=	stress intensity factor for emergency/faulted loading conditions (worst-case loading)
$K_{Ic}(HT)$	=	fracture toughness at time of hydro test
$K_{Ic}(E/F)$	=	fracture toughness at time t(HT) + Δt for emergency/faulted conditions
t(HT)	=	time of hydro test
Δt	=	time between hydro tests

As indicated by the above equations and definitions, the worst-case loading condition is defined as the "emergency/faulted" (E/F) condition (terminology used in the ASME Code^{3,4}), and the operating time associated with the E/F condition is just prior to the next hydro test, when the vessel degradation (radiation and crack growth) taking place between hydro tests is a maximum.

The E/F conditions include the maximum credible primary-system pressure $[p(E/F)]$ and the minimum credible vessel temperature $[T_v(E/F)]$ and any less extreme values of $p(E/F)$ and $T_v(E/F)$ that maximize the ratio $K_1(E/F)/K_{lc}(E/F)$ during operation of the reactor. As illustrated in Ref. 6, increasing $p(E/F)$ increases $K_1(E/F)$, and decreasing $T_v(E/F)$ decreases $K_{lc}(E/F)$. For most cases of interest, the maximum value of $p(E/F)$ is related to the limiting pressure established by the safety-value/rupture-disc system.

The terms in Eqs. (1) and (2) are further defined as follows:

$$K_1(HT) = [p(HT) + S] (c_m s_m + s_b s_b) \sqrt{\pi a} \quad (3)$$

$$K_1(E/F) = [p(E/F) + S] (c_m s_m + c_b s_b) \sqrt{\pi(a + \Delta a)} \quad (4)$$

$$S = c_r \sigma_r / (c_m s_m + c_b s_b) \quad (5)$$

$$s = \sigma/p \quad (6)$$

$$K_{lc}(HT) = A + B \exp \{C[T_v(HT) - RT_{NDT}(HT)]\} \quad (7)$$

$$K_{lc}(E/F) = A + B \exp \{C[T_v(E/F) - RT_{NDT}(E/F)]\} \quad (8)$$

$$RT_{NDT}(HT) = RT_{NDT_0} + \Delta RT_{NDT}(HT) \quad (9)$$

$$RT_{NDT}(E/F) = RT_{NDT_0} + \Delta RT_{NDT}(E/F) \quad (10)$$

$$\Delta RT_{NDT}(HT) = e \cdot \Delta NDTT \cdot t(HT) \quad (11)$$

$$\Delta RT_{NDT}(E/F) = e \cdot \Delta NDTT \cdot [t(HT) + \Delta t], \quad (12)$$

where

a	=	crack dimension
Δa	=	increase in a during Δt
c	=	crack characterization factor
b, m, r	=	bending, membrane, residual
σ	=	far-field stress
$p(HT), p(E/F)$	=	primary-system pressure for hydro-test and emergency/faulted conditions
A, B, C	=	constants
e	=	uncertainty factor for $\Delta NDTT$
$\Delta NDTT$	=	rate of increase of nil-ductility transition temperature
RT_{NDT}	=	nil-ductility reference temperature
RT_{NDT_0}	=	initial value of RT_{NDT}
$T_v(HT), T_v(E/F)$	=	vessel temperature for hydro test and emergency/faulted conditions

An uncertainty factor could be applied to the right-hand side of Eq. (1) to account for the inequality, but it is more precise to specify equality, use an uncertainty factor of 1.0, and apply uncertainty factors to individual terms in Eqs. (1) through (12), where applicable.

Parameters that require uncertainty factors are $K_I(HT)$, $K_I(E/F)$, $K_{Ic}(HT)$, $K_{Ic}(E/F)$, RT_{NDT_0} , a , Δa , and $\Delta NDTT$. The uncertainty factors for the $K_I(HT)$ and $K_I(E/F)$ equations are the same, and thus, as indicated by Eq. (2) they cancel out. Uncertainties in c , σ , a , and Δa , however, must be considered. If the flaw does not grow substantially during Δt , c doesn't change, $\Delta a \approx 0$, and a cancels out (it is shown in Sect. 5 that flaw growth during Δt is insignificant). The uncertainty in σ is accommodated by using upper-bound values.

An uncertainty factor applied directly to K_{Ic} also cancels, but individual factors can be applied to the constants A , B , and C , which results in a change in the shape of the curve. The smallest value of Δt is obtained using a curve with a steep transition region and also a curve that is shifted to the left. Both the shape and shift must be considered to account for scatter in the data (upper- and lower-bound curves may be different). The ASME (EPRI)* lower-bound K_{Ic} curve exhibits a rather steep transition compared to that for more recently suggested K_{Ic} curves, as indicated in Fig. 1 (Fig. 13 of Ref. 8). If the ASME(EPRI) lower-bound curve is multiplied by a factor to obtain a mean curve, as it was in the IPTS studies,⁹ and if an upper-bound curve is obtained in the same way, a realistic shift in the curve is obtained (Fig. 2, which is Fig. 5 in Ref. 8, with the upper-bound curve added), and a rather steep transition is considered. An additional benefit to obtaining the range of curves this way is that there is no sensitivity of hydro-test conditions to the specific curve used (lower-bound, upper-bound, or anything in between) because the multiplying factors on K_{Ic} cancel [Eq. (2)]. This approach seems reasonable and conservative and will be used henceforth without the need to include additional variations such as the "new mean" curve considered in Refs. 6 and 7.

As indicated in Fig. 2, one of the three suggested lower-bound curves is referred to as the ASME curve and another as the EPRI curve. The EPRI curve has recently been accepted as the official ASME curve, and thus the identifier ASME (EPRI) in this report.

Because $K_{Ic} = f(T - RT_{NDT})$, uncertainties in RT_{NDT_0} and $\Delta NDTT$ must also be considered, as mentioned above. Equations (11) and (12) include an uncertainty factor (e) for $\Delta NDTT$, but in Refs. 2, 6, and 7, the uncertainty in RT_{NDT_0} was accounted for in a safety factor (SF) applied to Δt . If the uncertainty in RT_{NDT_0} is included separately, then "all" uncertainties are addressed individually, and a safety factor applied to Δt is not necessary. When the best-estimate value of RT_{NDT_0} is used, a safety factor of 0.5 on Δt is recommended.⁶

As mentioned above, shifting the K_{Ic} curve to the left decreases the value of Δt (shifting the curve to the left increases the slope of the curve for a specific value of $T - RT_{NDT}$ and thus decreases the value of Δt required to decrease K_{Ic} by a specific amount). The curve is effectively shifted to the left by decreasing the value of RT_{NDT_0} . Thus, the uncertainty applied to RT_{NDT_0} should decrease RT_{NDT_0} .

The uncertainty in RT_{NDT_0} for nuclear pressure vessel steels can be obtained from Nuclear Regulatory Commission (NRC) studies pertaining to pressurized-water reactors (PWRs).¹⁰ The value used in the NRC IPTS studies⁹ was taken from Ref. 10 and is considered appropriate for the HFIR vessel material. One standard deviation (σ) for both weld and plate material was $\pm 17^\circ\text{F}$. In Ref. 10, 2σ was used in the deterministic analysis.

2.2 Effect of Revisions

The upgraded treatment of uncertainties described above is considered to be the most appropriate, and thus it is of interest to see if the method used in Refs. 6 and 7 is relatively conservative. This can be done by calculating Δt , given the other hydro-test conditions. The equation for Δt is obtained from Eq. (1) by back substituting Eqs. (3) through (12). The equation and appropriate input are included in Appendix A, and the results are presented in Fig. 3 (Δt vs e), Fig. 4. [Δt (min) vs RT_{NDT_0}], and Table A.1. As indicated in Fig. 3, and as discussed in Refs. 6 and 7, there are specific values of e that correspond to minimum values of Δt , and thus a reasonable range of values of e must be specified to define the minimum value of Δt . Because "correct" values of e cannot be defined, the minimum value of Δt within the specified range of e values must be used as a hydro-test condition.

The results in Figs. 3 and 4 are based on $p(\text{HT}) = 825$ psid, $T_v(\text{HT}) = 85^\circ\text{F}$, $p(\text{E/F}) = 679$ psid, $T_v(\text{E/F}) = 80^\circ\text{F}$, $\Delta a = 0$, and $t(\text{HT}) = 26$ EFPY(100 MW). The values of RT_{NDT_0} used in the analysis were 10°F , which is the best-estimate value for the HB-3 nozzle weld, and $10 - 2 \times 17 = -24^\circ\text{F}$, which is the -2σ value. The corresponding values of $\Delta t(\text{min})$ from Figs. 3 and 4 are 4.9 and 2.7 EFPY(100 MW). Thus, the maximum permissible values of Δt for the hydro test are

$$RT_{NDT_0} = 10^\circ\text{F}: \Delta t = \Delta t(\text{min}) \text{ SF} = 4.9 \times 0.5 = 2.5 \text{ EFPY}(100 \text{ MW})$$

$$RT_{NDT_0} = -24^\circ\text{F}: \Delta t = 2.7 \times 1.0 = 2.7 \text{ EFPY}(100 \text{ MW})$$

As discussed above, a safety factor of 0.5 was used on $\Delta t(\text{min})$ in Refs. 6 and 7 with the best-estimate value of RT_{NDT_0} (10°F), and safety factors of 1.0 are appropriate when using the -2σ values of RT_{NDT_0} . It is apparent that the procedure for calculating Δt in Refs. 6 and 7 [best-estimate value of RT_{NDT_0} and safety factor on Δt (min) of 0.5] is relatively conservative.

3. TIME-DEPENDENCE OF HYDRO-TEST CONDITIONS

In Refs. 6 and 7, the reduction in fracture toughness due to radiation damage was calculated for conditions at the beginning of the specified period, which, for the life-extension study, is 26 EFPY(100 MW). A more precise and less conservative approach is to recalculate the hydro-test conditions for each new hydro test in order to take advantage of the reduced slope of the K_{Ic} vs $T - RT_{NDT}$ curve as RT_{NDT} increases with time. Also $p(HT)$ can be greater at the beginning of the life-extension period than at the end, when K_{Ic} has reached the "minimum permissible" value (probability of failure increases with decreasing K_{Ic}). These two modeling improvements will increase the average value of Δt , and this will reduce operating costs and increase the life extension. On the other hand, it may be that having different hydro-test conditions for each hydro test will be more of an operational problem than the potential cost saving is worth. Even so, a few calculations are included to illustrate the potential benefit.

The calculations are made using Fig. 5 (Fig. 7 of Ref. 7), which is a plot of the best-estimate conditional probability of vessel failure $[P(F|E)]$ and mean frequency of failure $[\phi(F)]$ vs reactor operating time, for several pressures and several event frequencies $[\phi(E)]$ (the event frequency for hydro testing is equal to $1/\Delta t$). According to the information in Fig. 5, if $\Delta t \leq 2.5$ EFPY(100 MW), a hydro-test pressure of 900 psid would be acceptable up to 41 EFPY(100 MW) [$p(HT) = 825$ psid and $\Delta t = 2.5$ EFPY(100 MW) were tentatively proposed in Ref. 7 for a life extension beyond 26 EFPY(100 MW) of ≈ 24 EFPY(100 MW)]. Although a larger value of $p(HT)$ could in principle be used up to ≈ 41 EFPY (limiting probability of failure is $1 \times 10^{-4} y^{-1}$), a value of 900 psid will be used for this illustrative problem.

The first step in the analysis is to calculate Δt (min) using $t(HT) = 26$ EFPY(100 MW). As indicated in Fig. 6 and Table A.2, Δt (min) = 5.1, and thus Δt (specified) = 5.1 EFPY(100 MW). For the next calculation, $t(HT) = 26 + 5.1 = 31.1$ EFPY(100 MW). This corresponds to Δt (min) = 6.1 (Fig. 6), and thus Δt (specified) = 6.1 EFPY(100 MW). For the next calculation, $t(HT) = 31.1 + 6.1 = 37.2$ EFPY(100 MW), and the corresponding value of Δt (min) is 7.3. Thus, Δt (specified) = 7.3 EFPY(10 MW), and the next value of $t(HT)$ is $37.2 + 7.3 = 44.5$ EFPY(100 MW). As indicated in Fig. 7, the increase in Δt (min) with increasing $t(HT)$ is essentially linear.

All of the above values of Δt (specified) are greater than 5 EFPY(100 MW), so a constant value of 5.0 EFPY(100 MW) would be acceptable. From Fig. 5, with reference to the $p(HT) = 900$ psid and $\phi(E) = 1/5 = 0.2 y^{-1}$ frequency-of-failure curve, the last hydro test would be run at 50 EFPY(100 MW) (point 7), and thus the life of the vessel would be 55 EFPY(100 MW).

As mentioned in Ref. 6, it may be prudent to limit Δt (specified) to 3.0 EFPY(100 MW) because of lingering concerns with regard to degradation mechanisms. When a value of Δt is specified, it is possible to calculate the corresponding value of $p(HT)$ as a function of e , using Eq. (14) of Ref. 6, which is

$$p(HT) = \frac{K_{Ic}(HT)}{K_{Ic}(E/F)} p(E/F) + S \left[\frac{K_{Ic}(HT)}{K_{Ic}(E/F)} - 1 \right] \quad (13)$$

or

$$p(HT) = [p(E/F) + S] \frac{K_{Ic}(HT)}{K_{Ic}(E/F)} - S, \quad (14)$$

where e is included in the expressions for K_{Ic} (see Eqs. 7-12).

A plot of $p(HT)$ vs e is included in Fig. 8 for $T_v(HT) = 85^\circ\text{F}$, $p(E/F) = 679$ psid, $T_v(E/F) = 80^\circ\text{F}$, $\Delta t = 3.0$ EFPY(100 MW), $RT_{NDT_0}(-2\sigma) = -24^\circ\text{F}$, $t(HT) = 26, 32$, and 38 EFPY(100 MW), and $\Delta a = 0$. As indicated, there is a maximum in the curve for $0.5 \leq e \leq 1.5$, and the maximum value is a function of $t(HT)$ (Fig. 9). The maximum or larger value must be specified for the hydro test because the "correct" value of e is not known. However, each time a test is conducted, a smaller value of $p(HT)$ can be used.

Suppose that $p(HT) = 850$ psid and $\Delta t = 3.0$ EFPY(100 MW) are specified for at least the first several hydro tests, starting with the one for $t(HT) = 26$ EFPY(100 MW). From Fig. 5, using $\phi(E) = 1/3.0 = 0.33 \text{ y}^{-1}$ and interpolating, a limiting value of 46 EFPY(100 MW) for $t(HT)$ is achieved. After operating an additional 3.0 EFPY(100 MW), the hydro-test pressure could be reduced from 850 to 825 psid and the vessel operated an additional 2.5 EFPY (100 MW) for a total life of ≈ 52 EFPY(100 MW).

4. PRESSURIZER-PUMP DEPENDENCE OF HYDRO-TEST CONDITIONS

Figure 9 indicates that values of $p(\text{HT})$ less than 825 psid could be used. However, according to Ref. 5, $p(\text{HT})$ must not be less than $1.25 p(\text{design})$, and at the present time $p(\text{design}) = 650$ psi (nominal safety-value/rupture disc specification). There are plans, however, to replace the present primary-system pressurizer pumps with pumps that have a maximum head capability of ≈ 580 psi. It may be that the probability of the primary-system pressure being greater than 580 psi is sufficiently small to allow using this pressure for $p(\text{E/F})$ and $p(\text{design})$. The result would be a permissible vessel lifetime greater than 50 EFPY(100 MW) or larger margins on $p(\text{HT})$ and/or Δt .

A lifetime greater than 50 EFPY(100 MW) might not be of any practical significance; even so, calculations were made to determine what the value would be for the following conditions: $T_v(\text{HT}) = 85^\circ\text{F}$, $p(\text{E/F}) = 580$ psid, $T_v(\text{E/F}) = 80^\circ\text{F}$, $RT_{NDT_o}(-2\sigma) = -24^\circ\text{F}$, $\Delta t = 3.0$ EFPY(100 MW), $T_v(\text{HT}) = 26$ EFPY(100 MW), and $\Delta a = 0$.

The first step in the above calculation is to determine $p(\text{HT})$ vs e using Eq. 16. The results are given in Fig. 10 and Table A.4, which indicate that $p(\text{HT})$ (max) = 720 psid. The minimum permissible value, as discussed above, is $1.25 \times 580 = 725$ psid; thus, $p(\text{HT}) = 725$ psid. Using Fig. 5 with $p(\text{HT}) = 725$ psid and $\phi(\text{E}) = 0.33\text{y}^{-1}$ and doing some interpolating, the life of the vessel is estimated to be well in excess of 60 EFPY(100 MW).

If the specified value of $p(\text{HT})$ is greater than the calculated value, the ratio of specified $p(\text{HT})$ to calculated $p(\text{HT})$ constitutes a safety factor applied to $K_{lc}(\text{HT})/K_{lc}(\text{E/F})$ and $\{[K_{lc}(\text{HT})/K_{lc}(\text{E/F})] - 1\}$ [Eq. (13)], or the safety factor on Δt will be larger. As an illustration, suppose $p(\text{HT}) = 850$ psid, $T_v(\text{HT}) = 85^\circ\text{F}$, $p(\text{E/F}) = 580$ psid, $T_v(\text{E/F}) = 80^\circ\text{F}$, $RT_{NDT_o}(-2\sigma) = -24^\circ\text{F}$, $T_v(\text{HT}) = 26$ EFPY(100 MW), and recall that for $\Delta t = 3.0$ EFPY(100 MW), $p(\text{HT}) = 720$ psid. From Fig. 11 and Table A.5, $\Delta t(\text{min}) = 7.64$ EFPY(100 MW). If the specified value of Δt is 3.0 EFPY (100 MW) as suggested above, the effective safety factor on Δt is $3.0/7.6 = 0.39$ and that on $p(\text{HT})$ is $850/720 = 1.2$. Based on the arguments in Sect. 2, a safety factor of 1.0 on Δt is adequate.

5. FLAW GROWTH

5.1 Mechanisms

The two most likely mechanisms for the growth of flaws in the HFIR vessel are corrosion and fatigue. Both of these mechanisms have been evaluated previously,^{1,2,11-14} and the effect of flaw growth on the calculation of Δt was also evaluated.¹⁴ These evaluations are reviewed and summarized in this report as a matter of convenience and completeness as the information relates to vessel life extension.

5.2 Corrosion

Corrosion of the vessel cladding material was investigated in and out of the reactor and was found to be acceptable.^{1,11} The low temperature ($\approx 120^\circ\text{F}$), very low concentration of chlorides and lack of sensitization of the cladding precludes stress corrosion cracking of the cladding. Even if a crack does exist in the cladding, for whatever reason, the purity of the water is such that electrolytic effects on corrosion in the base material would be very small. Furthermore, the localized corrosion of the base material would be of a non-cracking nature; that is, a smooth cavity would be generated, rather than a sharp crack. Thus, it was concluded that corrosion would not result in significant flaw growth.¹¹

5.3 Fatigue

5.3.1 Method of analysis

The analytical procedure for calculating flaw growth by fatigue is included in Section XI of the ASME Code.¹⁵ In that document, flaw growth per load cycle (da/dN) is calculated from

$$\frac{da}{dN} = m Q_2 \Delta K^n, \quad (15)$$

where

a	=	crack depth
N	=	number of load cycles
ΔK	=	$K_{max} - K_{min}$ = change in the stress intensity factor with application of load
K_{min}	=	minimum value of the stress intensity factor during load cycling
K_{max}	=	maximum value of the stress intensity factor during load cycling
Q_2	=	$k R + l$ for $0.25 \leq R \leq 0.65$
R	=	K_{min}/K_{max}
m, n	=	constants dependent on R , ΔK , type flaw, and environment
k, l	=	constants dependent on ΔK , type flaw and environment

From Refs. 2 and 12,

$$K_{\min} = K_r \quad (16)$$

$$K_{\max} = K_m + K_b + K_t + K_r \quad (17)$$

$$K = c \sigma \sqrt{\pi a}, \quad (18)$$

$$K_m = c_m p(n) s_m \sqrt{\pi a}, \quad (19)$$

$$K_b = c_b p(n) s_b \sqrt{\pi a}, \quad (20)$$

$$K_t = -c_t \times \frac{1.85 (T_C - T_P)}{25^*} \sqrt{\pi a}, \quad (21)$$

$$K_r = c_r \sigma_r \sqrt{\pi a}, \quad (22)$$

where

$c_{b,m,r,t}$	=	geometry factors for bending, membrane, residual, and thermal stresses
K_b	=	stress intensity factor due to pressure-induced bending stress
K_m	=	stress intensity factor due to pressure-induced membrane stress
K_r	=	stress intensity factor due to residual stress
K_t	=	stress intensity factor due to thermal stress
$p(n)$	=	normal operating pressure
$s_{b,m}$	=	$\sigma_{b,m}/p$ = stress per unit pressure
T_C	=	temperature of primary coolant in contact with vessel wall
T_P	=	temperature of pool water
σ	=	stress
σ_r	=	residual stress

Equation (15) is plotted in Fig. A-4300-1 of Ref. 15 and is included herein as Fig. 12. As indicated in Fig. 12, the curve for surface flaws exposed to water is bilinear, the intersection depending on the value of R for $0.25 \leq R \leq 0.65$ (as demonstrated later, R falls in this range). The value of ΔK corresponding to the intersection is obtained by setting the two da/dN equations equal to each other. This yields

$$1.01 \times 10^{-1} (3.75R + 0.06) \Delta K^{1.95} = 1.02 \times 10^{-6} (26.9R - 5.725) \Delta K^{5.95},$$

*In Ref. 2, this number was inadvertently printed as 50 instead of 25. No errors exist in Ref. 2 as a result of this "typo."

or

$$\Delta K = \left(\frac{1.01 \times 10^{-1} (3.75R + 0.06)}{1.02 \times 10^{-6} (26.9R - 5.725)} \right)^{1/4} \quad (23)$$

As will be demonstrated later, the value of ΔK is such that the upper portion of the bilinear curve in Fig. 12 is appropriate for the problem at hand. Thus,

$$\frac{da}{dN} = 1.01 \times 10^{-7} (3.75R + 0.06) \Delta K^{1.95} \quad (24)$$

Equation (24) must be multiplied by an average value of dN/dt to obtain the crack growth per unit of time (da/dt):

$$\frac{da}{dt} = \frac{da}{dN} \times \frac{dN}{dt}, \quad (25)$$

where dN/dt must be estimated from the operating history of HFIR.

5.3.2 Calculation of flaw growth by fatigue

The controlling flaw is an axially oriented inner surface flaw in the HB-3 nozzle weld directly above the nozzle center, and an appropriate flaw depth to consider is 1.0 in.^{2,6} Appropriate input data for Eqs. (16) through (22) for this flaw and the indicated loading conditions are included in Table 5.1, which is a summary of data from Ref. 12 and Ref. 2 [Table 5.1 and Appendix G (Case A)]. Applying these data yields

$$K_{\min} = K_r = 0.86 \times 8.5 \sqrt{\pi \times 1.0} = 13.0 \text{ ksi } \sqrt{\text{in.}}$$

$$\begin{aligned} K_{\max} &= 1.146 \times 16.82 \times 0.475 \sqrt{\pi \times 1.0} \\ &+ 0.741 \times 4.24 \times 0.475 \sqrt{\pi \times 1.0} \\ &+ 0.741 \left[-1.85 \frac{(120 - 95)}{25} \right] \sqrt{\pi \times 1.0} \\ &+ 0.86 \times 8.5 \sqrt{\pi \times 1.0} \\ &= 16.2 + 2.6 - 2.4 + 13.0 = 29.4 \text{ ksi } \sqrt{\text{in.}} \end{aligned}$$

Table 5.1. Input data for the calculation of flaw growth and Δt vs e

Parameter	Value
Fracture-toughness curve	ASME K_{Ic}
A , ksi $\sqrt{\text{in}}$	33.2
B , ksi $\sqrt{\text{in}}$	20.73
C , $^{\circ}\text{F}^{-1}$	0.0200
a , in.	1.0
c_b	0.741
c_m	1.146
c_r	0.860
s_b	4.24
s_m	16.8
σ_r , ksi	8.5
$p(n)$, psi	475
$p(HT)$, psi	900
$p(E/F)$, psi	679
$RT_{NDT_{\sigma}}$, $^{\circ}\text{F}$	10
$\Delta NDTT$, $^{\circ}\text{F}/\text{EFPY}(100 \text{ MW})$ ($a = 1.0 \text{ in.}$)	2.44
e	0.5–1.5
T_C , $^{\circ}\text{F}$	120
T_P , $^{\circ}\text{F}$	95
$T_v(HT)$, $^{\circ}\text{F}$	85
$T_v(E/F)$, $^{\circ}\text{F}$	80
$t(HT)$, EFPY(100 MW)	19.4
Δt , EFPY(100 MW)	dependent variable

Thus,

$$\Delta K = K_{\max} - K_{\min} = 29.4 - 13.0 = 16.4 \text{ ksi } \sqrt{\text{in.}}$$

$$R = \frac{K_{\min}}{K_{\max}} = \frac{13.0}{29.4} = 0.44$$

From Eq. (23), using $R = 0.44$,

$$\Delta K = 12.9 \text{ ksi } \sqrt{\text{in.}} < 16.4 \text{ ksi } \sqrt{\text{in.}}, \text{ justifying Eq. (26).}$$

From Eq. (24), with $R = 0.44$ and $\Delta K = 16.4 \text{ ksi } \sqrt{\text{in.}}$,

$$\frac{da}{dN} = 4.04 \times 10^{-5} \text{ in./cycle}$$

The number of load cycles was estimated from data in the HFIR quarterly reports from June 1972 to September 1987, during which time (184 months) there were 335 full-pressure cycles.¹⁶ Thus,

$$\frac{dN}{dt} = \frac{335}{184} \times 12 = 21.8 \text{ cycles/year},$$

and

$$\begin{aligned} \frac{da}{dt} &= 4.04 \times 10^{-5} \frac{\text{in.}}{\text{cycle}} \times 21.8 \frac{\text{cycles}}{\text{year}} \\ &= 8.8 \times 10^{-4} \text{ in./year} \end{aligned}$$

As time progresses, a will increase some because the increase in crack depth will increase the K_I value. The increase over the next 30 years, however, is negligible. In this regard, the selection of $a = 1.0 \text{ in.}$, as a starting value is justified on the basis that it is a best estimate of the largest flaw that could exist during the most recent hydro test [19.4 EFY(100 MW)] without propagation.

5.4 Maximum Flaw-Growth Rate Deduced from Hydro-Test Results

Another method of estimating flaw growth is to assume that at the time of the last hydro test [19.4 EFPY(100 MW)] a just-subcritical flaw existed. In Ref. 7, the "maximum" depth of a flaw that would be just barely subcritical is ≈ 1.5 in. Assuming the growth rate to be constant, its value is $1.5 \text{ in}/19.4 \text{ EFPY}(100 \text{ MW}) = 0.08 \text{ in/EFPY}(100 \text{ MW})$.

5.5 Calculation of Δt With Δa Included

5.5.1 Method of analysis

The effect of increasing crack depth, during Δt , on the calculated value of Δt can be determined by including the increase in crack depth in Eq. (22) of Ref. 6. This yields

$$\Delta t = \frac{E(E/F) - \frac{1}{C} \ln \left\{ \frac{A}{B} \left[\frac{K_{Ic}(HT)}{A} \left(\frac{p(E/F) + S}{p(HT) + S} \right) \left(\frac{a(\Delta t)}{a(HT)} \right)^{0.5} - 1 \right] \right\}}{e \cdot \Delta \dot{N}DTT}, \quad (26)$$

where

$a(\Delta t)$	=	$a(HT) + \dot{a} \Delta t$ = value of a at time $t(HT) + \Delta t$
$a(HT)$	=	crack depth at time of hydro test
$E(E/F)$	=	$T_v(E/F) - RT_{NDT_0} - e \cdot \Delta \dot{N}DTT \cdot t(HT)$
$E(HT)$	=	$T_v(HT) - RT_{NDT_0} - e \cdot \Delta \dot{N}DTT \cdot t(HT)$
e	=	uncertainty factor applied to $\Delta \dot{N}DTT$
$\Delta \dot{N}DTT$	=	rate of increase of nil ductility transition temperature
$K_{Ic}(HT)$	=	fracture toughness at time of hydro test = $A+B \exp [C \cdot E(HT)]$

5.5.2 Calculations

Appropriate input data for Eq. (26) are obtained from Table 5.1. Using these data and the value of da/dt from Sect. 5.3,

$$\Delta t = \frac{E(E/F) - 50.0 \ln \left\{ 1.602 \left[0.02469 K_{Ic}(HT) (1 + 8.8 \times 10^{-4} \Delta t)^{0.5} - 1 \right] \right\}}{2.44e}, \quad (27)$$

where

$E(HT)$	=	$75 - 47.3e$
$E(E/F)$	=	$70 - 47.3e$
$K_{Ic}(HT)$	=	$33.2 + 20.73 \exp [0.020 E(HT)]$

If the "maximum" growth rate from Sect. 5.4 is considered, Eq. (26) becomes

$$\Delta t = \frac{E(E/F) - 50.0 \ln \left\{ 1.602 \left[0.02469 K_{Ic}(HT) (1 + 8.0 \times 10^{-2} \Delta t)^{0.5} - 1 \right] \right\}}{2.44e}, \quad (28)$$

where

$$\begin{aligned} E(HT) &= 75 - 47.3e \\ E(E/F) &= 70 - 47.3e \\ K_{Ic}(HT) &= 33.2 + 20.73 \exp [0.02E(HT)] \end{aligned}$$

Values of Δt vs e from Eqs. (27) and (28) are included in Table 5.2 along with results obtained letting $\Delta a = 0$, that is, neglecting crack growth.

Table 5.2. Calculated values of Δt for $\dot{a} = 0$, $\dot{a} = 8.8 \times 10^{-4}$ in./year, and $\dot{a} = 8.0 \times 10^{-2}$ in./year

e	Δt , EFPY (100 MW)		
	$\dot{a} = 0$	$\dot{a} = 8.8 \times 10^{-4}$ in./year	$\dot{a} = 8.0 \times 10^{-2}$ in./year
0.5	9.6	9.3	2.7
1.0	6.7	6.5	2.5
1.5	6.7	6.6	2.5

The results in Table 5.2 indicate that for the value of $\dot{a}$ derived from the ASME Code fatigue curves (8.8×10^{-4} in./year) the decrease in $\Delta t(\text{min})$, compared to using $\dot{a} = 0$, is only 3%, which is negligible. For the value of $\dot{a}$ determined by the hydro test (8.0×10^{-2} in./year), which is a factor of 10^2 greater than the "code" value, $\Delta t(\text{min})$ is a factor of 2.7 less than that for $\dot{a} = 0$. For the case depicted in Fig. 3 [$p(\text{HT}) = 825$ psid, $T_v(\text{HT}) = 85^\circ\text{F}$, $p(\text{E/F}) = 679$ psid, $T_v(\text{E/F}) = 80^\circ\text{F}$, $RT_{NDT_0}(-2\sigma) = -24^\circ\text{F}$, and $t(\text{HT}) = 26$ EFPY(100 MW)], application of this factor (2.7) would reduce $\Delta t(\text{min})$ from 2.7 to 1.0 EFPY(100 MW). If $p(\text{HT})$ were increased from 825 to 900 psid (Fig. 6), $\Delta t(\text{min})$ would be reduced from 5.1 to 1.9 EFPY(100 MW). This latter value of $\Delta t(\text{min})$ is probably reasonable from an operational point of view, and, as mentioned above and indicated in Fig. 6, $\Delta t(\text{min})$ increases with increasing $t(\text{HT})$.

The use of a value of $\dot{a}$ "confirmed" by the hydro test certainly has appeal. However, because it is 100 times greater than the "Code" fatigue value, it is considered to be unnecessarily conservative. A more realistic factor is 10, and this factor was used in the calculation of $\Delta t(\text{min})$ for $p(\text{HT}) = 900, 850$, and 825 psid, $T_v(\text{HT}) = 85^\circ\text{F}$, $p(\text{E/F}) = 679$ psi, $T_v(\text{E/F}) = 80^\circ\text{F}$, $RT_{NDT_0}(-2\sigma) = -24^\circ\text{F}$, and $t(\text{HT}) = 26$ EFPY (100 MW). The results are presented in Fig. 13 and Table B.1. The values of $\Delta t(\text{min})$ for $p(\text{HT}) = 900, 850$, and 825 psid are 4.4,

3.0, 2.4 EFPY (100 MW). In Ref. 7 and Sect. 2.2 of this report, which used a safety factor of 0.5 on $\Delta t(\min)$ to account for uncertainties in RT_{NDT_0} and $\dot{a}$ [$\dot{a}$ (best estimate) = 0], Δt (specified) = 2.5 EFPY(100 MW), compared to the more precisely determined value of 2.4 EFPY(100 MW). Thus, the recommended safety factor of 0.5 for the particular model used was reasonable. Even so, it is recommended that the above more precise model, which includes the -2σ value of RT_{NDT_0} , the “+2 σ ” value of $\dot{a}$, and a safety factor of 1.0 on Δt , be considered in the future.

6. CONSIDERATION OF GAMMA CONTRIBUTION TO RADIATION EMBRITTLEMENT

There has been a continuous effort since HFIR was shut down in November 1986, for reevaluation of vessel integrity, to explain the apparent accelerated rate of embrittlement of the HFIR vessel materials.^{2,17-21} The conclusion from the most recent study²¹ is that the HFIR surveillance data^{2,6} and test data from materials testing reactors (Oak Ridge Research Reactor, Materials Testing Reactor, etc.) correlate quite well with total (neutron and gamma) dpa (Fig. 14). At irradiation positions in materials testing reactors, the ratio of gamma to neutron dpa is quite small, and thus the gamma contribution to embrittlement is also quite small. At the HFIR vessel wall, however, the ratio is relatively large because the 3 ft of beryllium and water between the core and vessel is a very good fast neutron shield but a poor gamma shield. The ratio is sufficiently large in some areas of the vessel wall for gamma-induced embrittlement to dominate.

The substitution of total dpa for fast-neutron fluence in the vessel integrity evaluation does not, of course, change the values of $\Delta NDTT$ deduced from the HFIR surveillance data. All it does, potentially, is to change the factor used to transpose the surveillance data from its location of origin, which is very close to the vessel wall, to the tip of the flaw that is assumed to exist in the vessel wall for the sake of performing a fracture-mechanics analysis.

There are, of course, significant differences in the attenuation of neutrons and gammas in water and steel, and there may also be differences with regard to the effect of the beam tubes on neutron and gamma fluxes at the vessel wall. Eventually, three-dimensional transport calculations will be performed to determine the spatial distribution of total dpa, and these can be used to transpose the damage rates from surveillance-specimen locations to the tip of the assumed flaws. In the meantime, "conservatively" estimated gamma dpa distributions and existing fast neutron flux distributions² have been used in a reevaluation of the probability of vessel failure, and this evaluation is discussed briefly below.

A particular concern with regard to the gamma contribution is the lack of attenuation of the gamma dpa over the inner surface of the vessel wall moving away from the HB-1, 2, 3, and 4 nozzles. The lack of attenuation, compared with neutrons, tends to subject much more of the vessel wall to significant radiation damage. This tends to increase the probability of failure of the vessel because the probability is directly proportional to the number of flaws in the high-damage areas, and the number of flaws increases with increasing area considered.

The effective area of the beltline region is about 100 times the area of the nozzle welds, which dominate the calculated probability of failure when the influence of gammas is not considered. The stresses and total dpa for the beltline, however, are sufficiently less than for the nozzle welds that the net calculated increase in the probability of failure due to consideration of gammas [total $(n+\gamma)$ dpa compared to fast-neutron flux] and inclusion of more realistic high-damage areas around the nozzles is only 40% greater than calculated in Ref. 7, assuming all the damage is due to fast neutrons. Details of the evaluation are included in Appendix C.

The inclusion of gammas results in somewhat different values of $\Delta NDTT$ at the crack tip in some critical areas. However, this does not affect the calculation of the hydro-test conditions. As explained in Refs. 6 and 7 and in Sect. 2 of this report, there is a minimum (zero derivative) in the curve of Δt vs e and a maximum in the curve of $p(HT)$ vs e , and either the minimum value of Δt or the maximum value of $p(HT)$ is specified for the hydro test, irrespective of the corresponding values of e . Thus, hydro-test conditions are not affected by the gamma issue.

7. SUMMARY, CONCLUSIONS, AND RECOMMENDATIONS

The purpose of this report was to review and update some specific aspects of the HFIR vessel life-extension evaluation, including hydro-test conditions, presented in Ref. 7.

In Ref. 7, the uncertainties in RT_{NDT_0} and $\dot{a}$ were accounted for in a safety factor applied to $\Delta t(\text{min})$. A more precise way is to apply appropriate factors to the individual parameters. The individual factors were derived and applied in this report, and it was concluded that the safety factor previously applied to Δt was a reasonable choice. Even so, the use of individual factors is more precise and thus should be used in future analyses.

Most of the preliminary calculations pertaining to hydro-test conditions assumed $\dot{a} = 0$ because the estimated value was considered negligible. The calculation of $\dot{a}$ was reviewed and extended and an additional uncertainty factor applied. Using the ASME Code approach and fatigue data, the inclusion of $\dot{a}$ in the calculation of hydro-test conditions reduces Δt by only 3%. If a factor of 10 is applied to the Code-derived value of $\dot{a}$, and if an appropriate uncertainty factor is applied to RT_{NDT_0} instead of applying a safety factor to Δt , for a specific case considered, Δt was reduced by 4%. Thus, the recommended changes in handling uncertainties in RT_{NDT_0} and $\dot{a}$ have little effect on hydro-test conditions and thus on the calculated vessel lifetime.

A number of variations in hydro-test conditions was considered, but these variations had little effect on vessel lifetime. Even so, they introduce some flexibility that may be advantageous from an operational point of view.

Reducing $p(E/F)$ will, of course, have a significant effect on vessel lifetime. However, the calculated permissible lifetime [≈ 50 EFPY(100 MW)] without reducing $p(E/F)$ below the presently specified value (679 psid) appears to be well in excess of what will be required.

During the early part of the life-extension period, it is possible to use a relatively large value of $p(\text{HT})$ and thus relatively large value of Δt , which tends to reduce operating costs. However, as mentioned in Ref. 7, it seems prudent to limit Δt to ≈ 3.0 EFPY(100 MW) because of lingering concerns about flaw growth, particularly, and to some extent embrittlement. Certainly, the cost advantage of increasing Δt decreases with increasing Δt , and the presently proposed increase from 1.0 EFPY(85 MW) to 3.0 EFPY(100 MW) represents a substantial cost savings.

Using the upgraded method of analysis described in this report with $\Delta t = 3.0$ EFPY(100 MW), $T_v(\text{HT}) = 85^\circ\text{F}$, $p(E/F) = 679$ psid, $T_v(E/F) = 80^\circ\text{F}$, $RT_{NDT_0}(-2\sigma) = -24^\circ\text{F}$, $\dot{a} = 8.8 \times 10^{-3}$ in/year, and $t(\text{HT}) = 26$ EFPY(100 MW) the hydro-test pressure [$p(\text{HT})$] is calculated to be 850 psid. If these hydro-test conditions are maintained throughout the life of the vessel, a total vessel life of ≈ 46 EFPY(100 MW) is achieved. Toward the end of life, $p(\text{HT})$ can be reduced to maintain an acceptably low calculated probability of failure for the hydro-test condition. By this means a vessel total life of ≈ 50 EFPY(100 MW) can be achieved.

The most recent explanation for the HFIR vessel accelerated damage rate discovered in 1986 is that gamma radiation is responsible for a large fraction of the measured damage. It appears that the HFIR surveillance data correlate well with the total (neutron + gamma) dpa. Using this correlation, the effect of gammas on the spatial distribution of damage was included in the evaluation of vessel life extension. This and some related refinements in modeling resulted in a 40% increase in the calculated probability of failure, which is considered negligible and easily accommodated by an equal increase in the previously specified limiting value of the probability of failure, which was quite conservative.

Fig. 1. A Weibull analysis of the EPRI data base using the Wallin method gives a 99% confidence curve that bounds all the data (Ref. 8).

Fig. 2. Fracture toughness vs temperature for nuclear pressure vessel steels, indicating reasonable upper and lower bounds to data (Fig. 5 of Ref. 8 with upper-bound curve added).

$p(\text{HT}) = 825 \text{ psid}$
 $T(\text{HT}) = 85^\circ\text{F}$
 $p(\text{E/F}) = 679 \text{ psid}$
 $T(\text{E/F}) = 80^\circ\text{F}$
 $t(\text{HT}) = 26 \text{ EFPY}(100\text{MW})$
 $S = 326 \text{ psi}$

Fig. 3. Δt vs e for $t(\text{HT}) = 26 \text{ EFPY}(100 \text{ MW})$ and $RT_{NDT_0} = +10$ and -24°F

$p(\text{HT}) = 825 \text{ psid}$
 $T(\text{HT}) = 85^\circ\text{F}$
 $p(\text{E/F}) = 679 \text{ psid}$
 $T(\text{E/F}) = 80^\circ\text{F}$
 $S = 326 \text{ psi}$
 $t(\text{HT}) = 26 \text{ EFPY}(100\text{MW})$

Fig. 4. $\Delta t(\text{min})$ vs RT_{NDT_0} for $t(\text{HT}) = 26 \text{ EFPY}(100 \text{ MW})$

Fig. 5. Probability of failure vs operating time for hydro test and emergency/faulted conditions (Fig. 7, Ref. 7; numbers on curves refer to specific cases discussed in Ref. 7).

$p(\text{HT}) = 900 \text{ psid}$
 $T(\text{HT}) = 85^\circ \text{ F}$
 $p(\text{E/F}) = 679 \text{ psid}$
 $T(\text{E/F}) = 80^\circ \text{ F}$
 $RT_{\text{NDT}_0}(-2\sigma) = -24^\circ \text{ F}$
 $S = 326 \text{ psi}$

Fig. 6. Δt vs e for $t(\text{HT}) = 26 + \sum_i \Delta t(\text{min})_i$.

$p(\text{HT}) = 900 \text{ psid}$
 $T(\text{HT}) = 85^\circ \text{ F}$
 $p(\text{E/F}) = 679 \text{ psid}$
 $T(\text{E/F}) = 80^\circ \text{ F}$
 $RT_{\text{NDT}_0}(-2\sigma) = -24^\circ \text{ F}$
 $S = 326 \text{ psi}$

Fig. 7. $\Delta t(\text{min})$ vs $t(\text{HT})$.

Fig. 8. $p(HT)$ vs e .

Fig. 9. $p(HT)(max)$ vs $t(HT)$.

Fig. 10. $p(HT)$ vs e for $\Delta t = 3.0$ EFPY(100 MW), $p(E/F) = 580$ psid.

Fig. 11. Δt vs e for $p(HT) = 850$ psid, $p(E/F) = 580$ psid, $t(HT) = 2.6$ EFPY(100 MW).

Fig. 12. Reference fatigue crack growth curves for carbon and low alloy ferritic steels (Ref. 16).

Fig. 13. Δt vs e with $\dot{a} = 8.8 \times 10^{-3} \text{ in/y}$

Fig. 14. Charpy Δ NDTT vs dpa: neutron plus gamma dpa (all energies) for HFIR surveillance materials; neutron dpa ($E > 0.1$ MeV) for materials irradiated in ORR (Ref. 21).

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APPENDIX A

Calculation of Δt vs e and $p(HT)$ vs e for $\dot{a} = 0$ A.1 Equations for Δt vs e and $p(HT)$ vs e

The equation for calculating Δt is derived in Ref. 6 and is as follows:

$$\Delta t = \frac{E(E/F) - \frac{1}{C} \ln \left\{ \frac{A}{B} \left[\frac{K_{lc}(HT)}{A} \left(\frac{p(E/F) + S}{p(HT) + S} \right) - 1 \right] \right\}}{e \cdot \Delta \dot{N} D T T}, \quad (A.1)$$

where

$$K_{lc}(HT) = A + B \exp[C \cdot E(HT)], \quad (A.2)$$

$$E(HT) = T_v(HT) - RT_{NDT_0} - e \cdot \Delta \dot{N} D T T \cdot t(HT), \quad (A.3)$$

$$E(E/F) = T_v(E/F) - RT_{NDT_0} - e \cdot \Delta \dot{N} D T T \cdot t(HT), \quad (A.4)$$

A, B, C	=	constants
RT_{NDT_0}	=	initial (no damage) value of the nil-ductility reference temperature
$\Delta NDTT$	=	increase in nil-ductility transition temperature
$\Delta \dot{N} D T T$	=	time-averaged rate of $\Delta NDTT$
e	=	uncertainty factor associated with $\Delta \dot{N} D T T$
Δt	=	time between successive hydro tests
$t(HT)$	=	total operating time up to time of hydro test
$T_v(HT)$	=	temperature of vessel during hydro test
$T_v(E/F)$	=	emergency/faulted (lowest) operating temperature of vessel (assumed to exist at any time during Δt)
$p(HT)$	=	hydro-test pressure
$p(E/F)$	=	emergency/faulted (highest) operating pressure of vessel (assumed to exist any time during Δt)
S	=	$c_r \sigma / (c_m s_m + c_b s_b)$
c	=	crack characterization factor
s	=	σ/p

p	=	primary-system pressure
σ	=	far-field stress
m, b, r	=	membrane, bending, residual
a	=	crack dimension

Note that some of the nomenclature has been updated in this report relative to that in Ref. 6. The changes are as follows: $E(E/F)$ for $E(\Delta t)$, $K_{Ic}(E/F)$ for $K_{Ic}(\Delta t)$, $p(E/F)$ for $p(SV)$, $t(HT)$ for $\Delta t(HT)$, and $T_v(E/F)$ for $T_v(\Delta t)$. The identifier E/F refers to emergency/faulted conditions, while SV referred to the maximum pressure associated with the safety-valve/rupture-disc system. $T_v(\Delta t)$ referred to the minimum vessel temperature at time $t(HT) + \Delta t$. The value of $p(E/F)$ is the maximum credible and $T_v(E/F)$ the minimum credible during Δt . Thus, $p(E/F) = p(SV)$ and $T_v(E/F) = T_v(\Delta t)$.

A list of typical input for Eqs. (A.1) through (A.4) is included in Table A.1.

The equation for $p(HT)$ is derived in Ref. 6 and is included in the text of this report as Eq. (14). It is repeated here for convenience.

$$p(HT) = [p(E/F) + S] \frac{K_{Ic}(HT)}{K_{Ic}(E/F)} - S, \quad (A.5)$$

where

$$\begin{aligned} K_{Ic}(HT) &= \text{fracture toughness at time } t(HT) \\ K_{Ic}(E/F) &= \text{fracture toughness at time } t(HT) + \Delta t \\ &= A + B \exp [C \cdot E'(E/F)] \quad (A.6) \\ E'(E/F) &= T_v(E/F) - RT_{NDT_0} - e \cdot \Delta NDTT [t(HT) + \Delta t] \quad (A.7) \end{aligned}$$

Table A.1. Input data for the calculation of Δt and $p(HT)$ vs e

Parameter	Value
Fracture-toughness curve	ASME K_{Ic}
A , ksi $\sqrt{in}$	33.2
B , ksi $\sqrt{in}$	20.73
C , $^{\circ}F^{-1}$	0.0200
a , in.	1.0
c_b	0.741
c_m	1.146
c_r	0.860
s_b	4.24
s_m	16.8
σ_r , ksi	8.5
$p(HT)$, psi	900
$p(E/F)$, psi	679
$RT_{NDT\sigma}$, $^{\circ}F$	10, -14, -24
$\Delta \dot{NDTT}$, $^{\circ}F/EPY(100\text{ MW})$ ($a = 1.0\text{ in.}$)	2.44
e	variable
$T_v(HT)$, $^{\circ}F$	85
$T_v(E/F)$, $^{\circ}F$	80
$t(HT)$, EPY(100 MW)	19.4
Δt , EPY(100 MW)	dependent variable

- A.2 Calculation of Δt vs e for $p(\text{HT}) = 825$ psid, $T_v(\text{HT}) = 85^\circ\text{F}$, $P(\text{E/F}) = 679$ psid, $T_v(\text{E/F}) = 80^\circ\text{F}$, $t(\text{HT}) = 26$ EFPY(100 MW), $RT_{NDT_o} = +10$ and -24°F , and $S = 326$ psi.

$$\Delta t = \frac{E(\text{E/F}) - 50 \ln \{1.602[0.02630 K_{Ic}(\text{HT}) - 1]\}}{2.44e}$$

$$\begin{aligned} K_{Ic}(\text{HT}) &= 33.2 + 20.73 \exp [0.02E(\text{HT})] \\ E(\text{HT}) &= 85 - RT_{NDT_o} - 63.44e \\ E(\text{E/F}) &= 80 - RT_{NDT_o} - 63.44e \end{aligned}$$

Results are presented in Table A.2 and Figs. 3 and 4.

Table A.2. Δt vs e for $p(\text{HT}) = 825$ psid, $T_v(\text{HT}) = 85^\circ\text{F}$, $p(\text{E/F}) = 679$ psid, $T_v(\text{E/F}) = 80^\circ\text{F}$, $t(\text{HT}) = 26$ EFPY(100 MW), $RT_{NDT_o} = +10$ and -24°F , and $S = 326$ psi

e	Δt , EFPY(100 MW)	
	$RT_{NDT_o}, ^\circ\text{F}$	
	+10	-24
0.50	5.67	3.53
0.75	4.91	2.89
1.00	4.91	2.74
1.25	5.38	2.83
1.50	6.33	3.13
1.75	8.02	3.66
2.00	11.33	4.51

- A.3 Calculation of Δt vs e for $p(\text{HT}) = 900$ psid, $T_v(\text{HT}) = 85^\circ\text{F}$, $p(\text{E/F}) = 679$ psid, $T_v(\text{E/F}) = 80^\circ\text{F}$, $RT_{NDT_o}(-2\sigma) = -24^\circ\text{F}$, $t(\text{HT}) = 26 + \sum_i \Delta t_i \text{ EFPY}(100 \text{ MW})$, and $S = 326$ psi

$$\Delta t = \frac{E(\text{E/F}) - 50 \ln \{1.602[0.02469 K_{lc}(\text{HT}) - 1]\}}{2.44e}$$

$$\begin{aligned} K_{lc}(\text{HT}) &= 33.2 + 20.73 \exp [0.02E(\text{HT})] \\ E(\text{HT}) &= 109 - 2.44e \cdot t(\text{HT}) \\ E(\text{E/F}) &= 104 - 2.44e \cdot t(\text{HT}) \end{aligned}$$

Results are presented in Table A.3 and Figs. 6 and 7.

Table A.3. Δt vs e for $p(\text{HT}) = 900$ psid, $T_v(\text{HT}) = 85^\circ\text{F}$, $p(\text{E/F}) = 679$ psid, $T_v(\text{E/F}) = 80^\circ\text{F}$, $RT_{NDT_o}(-2\sigma) = -24^\circ\text{F}$, $t(\text{HT}) = 26 + \sum_i \Delta t_i(\text{min})_i$ EFPY(100 MW), and $S = 326$ psi

e	Δt , EFPY(100 MW)			
	$t(\text{HT})$, EFPY(100 MW)			
	26.0	31.1	37.2	44.5
0.50	7.24	7.68	8.30	9.18
0.75	5.67	6.32	7.31	8.89
1.00	5.15	6.13	7.77	10.82
1.25	5.16	6.67	9.59	16.68
1.50	5.59	8.05	14.08	
1.75	6.50	10.95		
2.00	8.19	19.07		

- A.4 Calculation of $p(\text{HT})$ vs e for $T_v(\text{HT}) = 85^\circ\text{F}$, $p(\text{E/F}) = 679$ psid, $T_v(\text{E/F}) = 80^\circ\text{F}$, $\Delta t = 3.0$ EFPY(100 MW), $RT_{NDT_0}(-2\sigma) = -24^\circ\text{F}$, $t(\text{HT}) = 26, 32, 38$ EFPY(100 MW), $S = 326$ psi.

$$\begin{aligned}
 P(\text{HT}) &= 1005 \frac{K_{lc}(\text{HT})}{K_{lc}(\text{E/F})} - 326 \\
 K_{lc}(\text{HT}) &= 33.2 + 20.73 \exp [0.02E(\text{HT})] \\
 K_{lc}(\text{E/F}) &= 33.2 + 20.73 \exp [0.02 E'(\text{E/F})] \\
 E(\text{HT}) &= 109 - 2.44e \cdot t(\text{HT}) \\
 E'(\text{E/F}) &= 104 - 2.44e [t(\text{HT}) + 3.0]
 \end{aligned}$$

Results are presented in Table A.4 and Figs. 8 and 9.

Table A.4. $p(\text{HT})$ vs e for $T_v(\text{HT}) = 85^\circ\text{F}$, $p(\text{E/F}) = 679$ psid, $T_v(\text{E/F}) = 80^\circ\text{F}$, $\Delta t = 3.0$ EFPY(100 MW), $RT_{NDT_0}(-2\sigma) = -24^\circ\text{F}$, $t(\text{HT}) = 26, 32$, and 38 EFPY(100 MW), $S = 326$ psi

e	$p(\text{HT}), \text{psid}$		
	$t(\text{HT}), \text{EFPY}(100 \text{ MW})$		
	26	32	38
0.50	814	808	802
0.75	828	815	803
1.00	833	812	792
1.25	830	801	775
1.50	821	784	754
1.75	806	765	735
2.00	789	747	719

- A.5 Calculation of $p(\text{HT})$ vs e for $T_v(\text{HT}) = 85^\circ\text{F}$, $p(\text{E/F}) = 580$ psid, $T_v(\text{E/F}) = 80^\circ\text{F}$, $RT_{NDT_0}(-2\sigma) = -24^\circ\text{F}$, $\Delta t = 3.0$ EFPY(100 MW), $t(\text{HT}) = 26$ EFPY(100 MW), $S = 326$ psi.

$$\begin{aligned}
 p(\text{HT}) &= 906 \frac{K_{Ic}(\text{HT})}{K_{Ic}(\text{E/F})} - 326 \\
 K_{Ic}(\text{HT}) &= 33.2 + 20.73 \exp [0.02E(\text{HT})] \\
 K_{Ic}(\text{E/F}) &= 33.2 + 20.73 \exp [0.02 E'(\text{E/F})] \\
 E(\text{HT}) &= 109 - 63.44e \\
 E'(\text{E/F}) &= 104 - 70.76e
 \end{aligned}$$

Results are presented in Table A.5 and Fig. 10.

Table A.5. $p(\text{HT})$ vs e for $T_v(\text{HT}) = 85^\circ\text{F}$, $p(\text{E/F}) = 580$ psid, $T_v(\text{E/F}) = 80^\circ\text{F}$, $RT_{NDT_0}(-2\sigma) = -24^\circ\text{F}$, $\Delta t = 3.0$ EFPY(100 MW), $t(\text{HT}) = 26$ EFPY(100 MW), $S = 326$ psi

e	$p(\text{HT})$ psid
0.50	702
0.75	714
1.00	719
1.25	716
1.50	708
1.75	695
2.00	679

- A.6. Calculation of Δt vs e for $p(\text{HT}) = 850$ psid, $T_v(\text{HT}) = 85^\circ\text{F}$, $p(\text{E/F}) = 580$ psid, $T_v(\text{E/F}) = 80^\circ\text{F}$, $RT_{NDT_0}(-2\sigma) = -24^\circ\text{F}$, $t(\text{HT}) = 26$ EFPY(100 MW), $S = 326$ psi.

$$\Delta t = \frac{E(\text{E/F}) - 50 \ln \{1.602[0.02321 K_{lc}(\text{HT}) - 1]\}}{2.44e}$$

$$\begin{aligned} K_{lc}(\text{HT}) &= 33.2 + 20.73 \exp [0.02E(\text{HT})] \\ E(\text{HT}) &= 109 - 63.44e \\ E(\text{E/F}) &= 104 - 63.44e \end{aligned}$$

Results are presented in Table A.6 and Fig. 11.

Table A.6. Δt vs e for $p(\text{HT}) = 850$ psid, $T_v(\text{HT}) = 85^\circ\text{F}$, $p(\text{E/F}) = 580$ psid, $T_v(\text{E/F}) = 80^\circ\text{F}$, $RT_{NDT_0}(-2\sigma) = -24^\circ\text{F}$, $t(\text{HT}) = 26$ EFPY(100 MW), $S = 326$ psi

e	Δt EFPY(100 MW)
0.50	10.96
0.75	8.49
1.00	7.65
1.25	7.64
1.50	8.32
1.75	9.91
2.00	13.38

APPENDIX B

Calculation of Δt vs e for $\dot{a} \neq 0$ B.1 Equations for Δt vs e

The equation for Δt with crack growth included is given in the text as Eq. (26).

$$\Delta t = \frac{E(E/F) - \frac{1}{C} \ln \left\{ \frac{A}{B} \frac{K_{Ic}}{A} \left(\frac{p(E/F) + S}{p(HT) + S} \right) \left(\frac{a(\Delta t)}{a(HT)} \right)^{0.5} - 1 \right\}}{e \cdot \Delta \dot{N} DTT}$$

$$\begin{aligned} a(\Delta t) &= a(HT) + \dot{a} \Delta t \\ K_{Ic}(HT) &= A + B \exp [C \cdot E(HT)] \\ E(E/F) &= T_v(E/F) - RT_{NDT_0} - e \cdot \Delta \dot{N} DTT \cdot t(HT) \\ E(HT) &= T_v(HT) - RT_{NDT_0} - e \cdot \Delta \dot{N} DTT \cdot t(HT) \end{aligned}$$

$T_v(HT)$, $T_v(E/F)$, $\Delta \dot{N} DTT$, and S are functions of a because of stress, temperature and fluence gradients in the wall. However, the functions are weak and thus neglected.

B.2 Calculation of Δt vs e for $p(HT) = 900, 850, \text{ and } 825 \text{ psid}$, $T_v(HT) = 85^\circ\text{F}$, $p(E/F) = 679 \text{ psid}$, $T_v(E/F) = 80^\circ\text{F}$, $RT_{NDT_0}(-2\sigma) = -24^\circ\text{F}$, $t(HT) = 26 \text{ EFY}(100 \text{ MW})$, $S = 326 \text{ psi}$, $a(HT) = 1.0 \text{ in}$, and $\dot{a} = 8.8 \times 10^{-3} \text{ in/year}$

Input not specified above is included in Table 5.1 of the text.

$p(HT) = 900 \text{ psid}$:

$$\Delta t = \frac{E(E/F) - 50 \ln \left\{ 1.602 \left[0.02469 K_{Ic}(HT) (1 + 8.8 \times 10^{-3} \Delta t)^{0.5} - 1 \right] \right\}}{2.44e}$$

$p(HT) = 850 \text{ psid}$:

$$\Delta t = \frac{E(E/F) - 50 \ln \left\{ 1.602 \left[0.02574 K_{Ic}(HT) (1 + 8.8 \times 10^{-3} \Delta t)^{0.5} - 1 \right] \right\}}{2.44e}$$

p(HT) = 825 psid:

$$\Delta t = \frac{E(E/F) - 50 \ln \left\{ 1.602 \left[0.02630 K_{Ic}(HT) (1 + 8.8 \times 10^{-3} \Delta t)^{0.5} - 1 \right] \right\}}{2.44e}$$

p(HT) = 900, 850, 825 psid:

$$K_{Ic} = 33.2 + 20.73 \exp [0.02 E(HT)]$$

$$E(E/F) = 104 - 63.44e$$

$$E(HT) = 109 - 63.44e$$

Results of the calculations are presented in Table B.1 and Fig. 13.

Table B.1. Δt vs e for p(HT) = 900, 850, and 825 psid, $T_v(HT) = 85^\circ\text{F}$, $p(E/F) = 679$ psid, $T_v(E/F) = 80^\circ\text{F}$, $RT_{NDT_0}(-2\sigma) = -24^\circ\text{F}$, $t(HT) = 26$ EFPY(100 MW), $S = 326$ psi, $a(HT) = 1.0$ in, and $\dot{a} = 8.8 \times 10^{-3}$ in/year

e	Δt , EFPY(100 MW)		
	p(HT), psid		
	900	850	825
0.50	5.77	3.82	2.82
0.75	4.75	3.22	2.44
1.00	4.41	3.05	2.36
1.25	4.43	3.12	2.46
1.50	4.76	3.40	2.71
2.00	6.45	4.59	3.72

APPENDIX C

Consideration of Gamma Contribution to Radiation-Induced Embrittlement

C.1. Introduction

In previous deterministic fracture-mechanics calculations,^{2,6} it was determined that the flaw location with the greatest potential for propagation of an assumed flaw was a portion of the HB-3 nozzle weld directly above the nozzle center (Figs. C.1, C.2, and C.3). This location has the worst combination of stress, RT_{NDT_0} and $\Delta NDTT$, although $\Delta NDTT$ at that location is not the maximum value in the vessel.

Hydro-test conditions were based on the HB-3 nozzle location for the flaw because the test conditions must satisfy the most limiting localized condition. In the probabilistic analysis, however, it is necessary to include the contribution of "all" areas of the vessel. Large areas tend to contribute more than small areas because for the same flaw surface density they contribute more flaws, and the probability of failure is directly proportional to the number of flaws. Mathematically speaking,

$$P(F/E)_T = N \sum_i A_i \hat{P}_i(F/E) , \quad (C.1)$$

where

$P(F/E)_T$	=	total conditional probability of failure
$\hat{P}_i(F/E)$	=	conditional probability of failure per flaw for the i th area
A_i	=	i th area of vessel wall
N	=	flaw surface density

On the other hand, a large area might have a relatively low value of stress and/or RT_{NDT_0} and/or $\Delta NDTT$ and thus not contribute much. This was the situation with the analysis in Refs. 2, 6, and 7, partly because of the steep attenuation of the fast neutron flux radially away from the beam tubes; that is, the damage appeared to be isolated to a small area around and including the beam-tube nozzles, and the damage was substantially greater for HB-2 and HB-3 than for HB-1 and HB-2. Furthermore, the stresses around the nozzles were relatively high because of residual stresses in the nozzle welds and discontinuity pressure-induced bending stresses close to the nozzle, and because the pressure-induced membrane stresses, which are the only stresses away from the nozzles within the length of the core, are rather low. For these reasons the weld areas around the nozzles accounted for all of the calculated probability of failure.

The gamma dpa is not attenuated in the azimuthal and vertical directions nearly as much as the fast neutron flux, and thus there is the possibility that a much larger area of the vessel (the entire beltline region) could contribute significantly to $P(F/E)_T$.

In Ref. 7, $\Delta NDTT$ values deduced from nozzle-weld surveillance specimens were transposed from a surveillance-capsule location close to HB-4 (Key 7, Position 8) to the tip of an assumed flaw in the nozzle weld above the HB-3 nozzle (Figs. C.1, C.2, and C.3). A more direct approach, which involves less potential for transposition inaccuracies, would be to transpose $\Delta NDTT$ values corresponding to Key 3 surveillance specimens because these specimens are much closer to the selected flaw location (HB-3 nozzle weld). However, it appeared that the damage per unit of fast-neutron fluence was significantly different for the different materials involved (nozzle material and nozzle weld), and, thus, the nozzle material (Key 3) could not be transposed to the nozzle weld position by simply using the ratio of fast-neutron fluxes.

The situation is different if all the data correlate with a single parameter, as it does with total dpa (Fig. 14). In the following detailed analysis, $\Delta NDTT$ values corresponding to Keys 1 and 4; 2; 3; and 5, 6, and 7 are transposed to nozzle welds for HB-1 and -4, HB-2, HB-3, and to the beltline shell, respectively, even though the materials for matching locations are not necessarily the same.

The purpose of the following analysis is to estimate the probability of vessel failure considering the reduction in K_{Ic} due to gammas as well as neutrons. Complete detailed distributions of the gamma fluxes are not yet available and thus "conservative" assumptions are made. The belt-line region of the vessel is divided into several sub-regions to properly account for the differences in dpa and stresses. Each subsection contributes to the probability of failure, as indicated by Eq. (C.1).

Detailed probabilistic fracture-mechanics calculations have thus far been performed only for the subregion directly above the HB-3 nozzle.⁷ Thus, in the present analysis, values of $\hat{P}(F|E)$ for each subregion are estimated by comparing the values of $\Delta NDTT$ and stress for each subregion with those for the subregion above HB-3, for which $\hat{P}(F|E)$ is known, and then using the previously calculated relationships between these two parameters and $\hat{P}(F|E)$ to obtain values of $\hat{P}(F|E)$ for the other subregions. These values and the corresponding values of N and A are then used in Eq. (C.1) to obtain $P(F|E)$. In so doing, it is assumed, as was the case for all previous analyses, that fluxes and $\Delta NDTT$ are constant with time. This is a reasonable assumption and makes transposition of $\Delta NDTT$ in space and $\Delta NDTT$ in time quite simple, as indicated in the following calculations.

Another assumption in the following analysis is that N is the same for all subregions. As discussed later, this represents a significant conservatism.

C.2 Calculations for HB-3 nozzle weld

Surveillance data were obtained from Positions 3, 5, and 8 of Key 3 in 1986. Test temperatures for each of the nine CVN specimens (each capsule contains three CVN specimens) were such that the derived value of $\Delta NDTT$ (113°F, Ref. 2, Table D.21) corresponded to the average total dpa for the three capsule locations. Because total-dpa distributions were not available, the average fast neutron flux was compared with the average for Positions 1 and 10, which represents the top of Key 3 (Fig. C.3) and thus the location of interest. The average for Positions 3, 5, and 8 = $(9.1 + 11.7 +$

17.8)/3 = 12.9, and the average for Positions 1 and 10 = (11.7 + 14.2) = 13.0. The total dpa would be more uniform, and thus the two averages are essentially equal. Therefore, for point A in Fig. C.2, $\Delta\dot{N}DTT = 113/17.53 = 6.4^\circ\text{F}/\text{EFPY}(100 \text{ MW})$.

The next step is to transpose $\Delta\dot{N}DTT$ at point A to point B. The data in Table 3 of Ref. 21 indicate that close to the HB-4 and HB-2 nozzles the ratio of gamma dpa to neutron dpa is 1.4 and 1.1, respectively (see Fig. C.2). Based on this information, a value of 1.3 is estimated for HB-3. Thus, at point A

$$\Delta\dot{N}DTT(\gamma, A) = 6.4 \times \frac{1.3}{2.3} = 3.6$$

$$\Delta\dot{N}DTT(n, A) = 6.4 \times \frac{1.0}{2.3} = 2.8$$

From Table E.18 of Ref. 2, the axial attenuation of the fast neutron flux from point A to point B (see Fig. C.2 for dimensions) is $1.4/6.4 = 0.22$. Assuming no attenuation of the gamma dpa from point A to B, the values of $\Delta\dot{N}DTT$ at point B are

$$\Delta\dot{N}DTT(\gamma, B) = 3.6$$

$$\Delta\dot{N}DTT(n, B) = 2.8 \times 0.22 = 0.62$$

From Table 5.2 of Ref. 2, the attenuation of the fast neutron flux from point B to point C is $2.58/2.68 = 0.96$ and from point C to point D, assuming $a = 1.0 \text{ in.}$, it is $1.71/1.97 = 0.87$. The attenuation of the gamma dpa from point B to point C is neglected, and from point C to point D, using an exponential attenuation factor of 0.66 in.^{-1} deduced from Fig. C.4, it is $e^{-0.66 \times 1.0} = 0.52$. Thus,

$$\Delta\dot{N}DTT(\gamma, D) = 3.6 \times 0.52 = 1.9$$

$$\Delta\dot{N}DTT(n, D) = 0.62 \times 0.96 \times 0.87 = 0.5$$

$$\therefore \Delta\dot{N}DTT(n+\gamma, D) = 1.9 + 0.5 = 2.4^\circ\text{F}/\text{EFPY}(100\text{MW})$$

This value agrees with what was used in Ref. 7.

The stress in the nozzle weld above the nozzle is the sum of the pressure-induced circumferential membrane and discontinuity bending stresses and circumferential residual stress. Moving around the nozzle toward the horizontal midplane (HMP) decreases the stresses normal to the flaw plane. At the HMP, the factor (ratio of stress at new location to the circumferential stress in the weld above HB-3) for an

axial flaw in the weld, neglecting the small thermal stress and assuming a pressure of 0.85 ksi (Ref. 2, Table 5.3), is

$$\frac{0.850 (12.49 - 3.27) + 1.7}{0.850 (16.82 + 4.24) + 8.5} = 0.36$$

For a circumferential flaw the factor is less. Counteracting the decrease in stress is an increase in $\Delta\dot{N}DTT$ on the counter-clockwise side (Fig. C.2, Key 3, Positions 8, 9, and 10). Because of the contribution of gammas, the increase is less than indicated by the recorded values of fast neutron flux. Neglecting this difference, the factor on $\Delta\dot{N}DTT$ for the HMP relative to that above HB-3, is $17.8/13.0 = 1.4$. On the other side of the nozzle the factor is $9.1/13.0 = 0.7$.

The factor on $P(F|E)$ for a factor of 0.36 on stress is $\sim 5 \times 10^{-3}$ (Ref. 2, Table G.3), and for a factor of 1.4 on $\Delta\dot{N}DTT$, or t , it is ~ 3 (Fig. 5). Thus, the total factor is $\sim 2 \times 10^{-2}$. At the opposite location, the total factor is $\sim 2 \times 10^{-3}$. In view of these very low values, a reasonable conservative total area (Eq. C.1) for the HB-3 nozzle weld to be used with the point value of $\dot{P}(F|E)$ at the top of the weld would be one half of the average "surface" area of the weld. Thus, with reference to Fig. C.2, A (HB-3 weld) = $\bar{W}\pi R_w = 1.0\pi \times 10/144 = 0.22 \text{ ft}^2$. (In Ref. 7, an area of 1.0 ft^2 was used to account for the contribution of the other nozzles.

C.3 Calculations for HB-2 nozzle weld

As indicated in Fig. 16, the calculated fast neutron flux at the center of the surveillance specimens is uniform around the HB-2 nozzle. From Table D.1 of Ref. 2, $\Delta\dot{N}TT$ [17.53 EFPY(100 MW)] = 117°F . Assuming $\Delta\dot{N}TT$ vs t to be linear and through the origin, $\Delta\dot{N}DTT = 117/17.53 = 6.7^\circ\text{F/EFPY}(100 \text{ MW})$.

The next step is to transpose the measured value of $\Delta\dot{N}DTT$ from point A to point B (Fig. C.2). As in the HB-3 calculation,

$$\Delta\dot{N}DTT(\gamma, A) = 6.7 \times \frac{1.1}{2.1} = 3.5$$

$$\Delta\dot{N}DTT(n, A) = 6.7 \times \frac{1.0}{2.1} = 3.2$$

Extrapolating the data in Table E.18 of Ref. 2, the axial attenuation of the fast neutron flux from point A to point B is $5.0/33 = 0.15$. Assuming no attenuation of the gamma dpa from point A to point B , the values of $\Delta\dot{N}DTT$ at point B are

$$\Delta\dot{N}DTT(\gamma, B) = 3.5$$

$$\Delta\dot{N}DTT(n, B) = 3.2 \times 0.15 = 0.48$$

The radial attenuation through water and steel is the same as for HB-3. Thus, at point D ,

$$\Delta \dot{NDTT}(\gamma, D) = 3.5 \times 0.52 = 1.8$$

$$\Delta \dot{NDTT}(n, D) = 0.48 \times 0.96 \times 0.87 = 0.4$$

$$\therefore \Delta \dot{NDTT}(n+\gamma, D) = 1.8 + 0.4 = 2.2^\circ F/EPY(100MW)$$

There is no variation of $\Delta \dot{NDTT}$ around HB-2, but the variation in stress is the same as for HB-3. For HB-3, most of the variation of $\dot{P}(F|E)$ around the nozzle was due to the variation in stress (factor of 5×10^{-3} for stress compared to 3 for $\Delta \dot{NDTT}$ on one side of the nozzle and 0.3 on the other). Thus, a factor of 2 on the area is also appropriate for the HB-2 nozzle weld area.

$$A(\text{HB-2 weld}) = 1.0\pi \times 15/144 = 0.33 \text{ ft}^2$$

C.4 Calculations for HB-1 and HB-4 nozzle welds

The fast neutron flux at the top of Keys 1 and 4 (HB-1, 4), which are mirror images of each other, is about one-third that at the top of Key 3 (HB-3) (Fig. C.3). However, the ratio of gamma dpa to neutron dpa for Keys 1 and 4 is somewhat greater than for Key 3 [1.4 compared to 1.3 (Fig. C.2)]. Thus, $\Delta \dot{NDTT}$ for the top of Keys 1 and 4 would be expected to be somewhat more than one-third of the corresponding value for Key 3. From Table D.21 of Ref. 2, the value of $\Delta \dot{NDTT}$ corresponding to the average of Positions 3 and 8 of Key 4 and Position 3 of Key 1 is

$$\Delta \dot{NDTT} (A105\Pi) = \frac{63}{17.53} = 3.6^\circ F/EPY(100MW)$$

This must be corrected for the difference in average flux in the surveillance capsules and that at the top of Keys 1 and 4. From Figs. C.2 and C.3, using the fluxes for the specimens that were removed in 1986 (Positions 3 and 8 in Key 4 and Position 3 in Key 1).

$$\Delta \dot{NDTT}(n+\gamma, A) = \frac{(5.17 + 4.16)/2}{(2 \times 7.27 + 3.34)/3} \times 3.6 = 2.8^\circ F/EPY(100 \text{ MW})$$

Separating the gamma and neutron contributions,

$$\Delta \dot{N}DTT(\gamma, A) = 2.8 \times \frac{1.4}{2.4} = 1.6$$

$$\Delta \dot{N}DTT(n, A) = 2.8 \times \frac{1.0}{2.4} = 1.2$$

As for HB-3, the axial attenuation of the fast neutron flux from point A to B is 0.22. Thus,

$$\Delta \dot{D}TT(\gamma, B) = 1.6$$

$$\Delta \dot{N}DTT(n, B) = 1.2 \times 0.22 = 0.26 ,$$

and

$$\Delta \dot{N}DTT(\gamma, D) = 1.6 \times 0.52 = 0.83$$

$$\Delta \dot{N}DTT(n, D) = 0.26 \times 0.96 \times 0.87 = 0.22$$

$$\therefore \Delta \dot{N}DTT(n+\gamma, D) = 0.83 + 0.22 = 1.1^\circ F/EPY(100MW)$$

The effective area for both HB-1 and HB-4 is the same as for HB-3. Thus, the total area for the HB-1 and 4 welds is $2 \times 0.22 = 0.44 \text{ ft}^2$.

C.5 Estimation of total $P(F|E)$ for the nozzle welds relative to value in Ref. 7

As indicated by Eq. (C.1),

$$P(F/E) \text{ (nozzle welds)} = \sum_i A_i \hat{P}_i(F/E) , \quad (C.2)$$

where i refers to each of the nozzle welds. The values of $\hat{P}(F|E)$ in Eq. (C.2) are specified as being relative to the total value given in Ref 7, in which case the flaw density (N) drops out. Thus, for the HB-3 weld, because $\Delta \dot{N}DTT(n+\gamma, D)$ is the same as that used in Ref. 7, and the stress factor was used only to deduce the damage area,

$$\hat{P}(F/E) (n+\gamma, D, HB-3) = 1.0$$

For HB-2,

$$\frac{\Delta \dot{NDTT}(n+\gamma, D, HB-2)}{\Delta \dot{NDTT}(n+\gamma, D, HB-3)} = \frac{2.2}{2.4} = 0.9$$

Thus, from Fig. 5, for $P(HT) = 900$ psi,

$$\hat{P}(F|E)(n+\gamma, D, HB-2) = \frac{P(F|E) (0.9 \times 50 \text{ EFPY})}{P(F|E) (50 \text{ EFPY})} = \frac{0.8 \times 10^{-5}}{1.1 \times 10^{-5}} = 0.7$$

For HB-1, 4,

$$\frac{\Delta \dot{NDTT}(n+\gamma, D, HB-1, 4)}{\Delta \dot{NDTT}(n+\gamma, D, HB-3)} = \frac{1.1}{2.4} = 0.5$$

Thus, from Fig. 5, for $p(HT) = 900$ psi,

$$\hat{P}(F|E)(n+\gamma, D, HB-1, 4) = \frac{P(F|E) (0.5 \times 50 \text{ EFPY})}{P(F|E) (50 \text{ EFPY})} = \frac{8.0 \times 10^{-7}}{1.1 \times 10^{-5}} = 0.07$$

The area used for the nozzle welds in Ref. 7 was 1.0 ft². Thus, the above areas represent values relative to those used in Ref. 7. Using Eq. (C.2),

$$\begin{aligned} P(F|E) (\text{nozzle welds}) &= (0.22 \times 1.0 + 0.33 \times 0.7 + 0.44 \times 0.07)/1.0 \\ &= 0.5 \end{aligned}$$

C.6 Calculation for beltline, exclusive of nozzle welds

A conservative approach to evaluating the contribution of the beltline to total $P(F|E)$ is to assume uniform gamma dpa and negligible neutron dpa over the beltline region, where the beltline area is

$$\begin{aligned} A(\text{beltline}) &\approx \pi D \times 2H \\ &= \pi \times 8 \times 2 \times 2 = 100 \text{ ft}^2, \end{aligned}$$

where D = diameter of vessel and H = height of fuel element (the appropriate value of the coefficient applied to H depends on the axial attenuation of the gamma dpa). Justification for ignoring the neutron dpa is given in Table 3 of Ref. 21, which indicates that at Key 7 Position 5 (Fig. C.2) the ratio of gamma dpa to neutron dpa

is quite large (5.1). Further from the nozzles, it would be even greater. The annular areas closer to and including the nozzle have significant neutron dpa values, but the stresses are low, with the exception of those in the nozzle welds, which are calculated separately, compared to those in the nozzle welds, and the areas are small compared to the total beltline area. Thus, these annular areas do not contribute significantly to the total $P(F|E)$.

Shell material (A212B) is/was located in Keys 5, 6, and 7, and specimens were withdrawn in 1983, 1986, and 1993. Most of the specimens were from positions close to the nozzles, and the corresponding test data were transposed to Key 7, Position 8 using the calculated fast neutron fluxes (this transposition procedure results in a high value of $\Delta\dot{N}DTT$ because the total dpa has less gradient than the fast neutron flux). The value of $\Delta\dot{N}DTT$ obtained is $4.6^\circ F/EPY(100 \text{ MW})$ (Table 2.6 of Ref. 7).

The ratio of gamma dpa to neutron dpa at Key 7, Position 8 is ~ 2.0 (deduced from Table 3 of Ref. 21). Thus,

$$\Delta\dot{N}DTT(\gamma, BL) = 4.6 \times \frac{2}{3} = 3.1^\circ F/EPY(100 \text{ MW})$$

This value must be attenuated 1.0 in. into the wall.

$$\begin{aligned}\Delta\dot{N}DTT(\gamma, BL, D) &= 3.1 \times e^{-0.66 \times 1.0} \\ &= 1.6^\circ F/EPY(100 \text{ MW})\end{aligned}$$

The corresponding value for the HB-3 nozzle weld is 2.4. A factor of $1.6/2.4 = 0.7$ on $\Delta\dot{N}DTT$, or t , is equal to a factor of $3.2 \times 10^{-6}/1.1 \times 10^{-5} = 0.3$ on $P(F|E)$ (Fig. 5).

The stresses for the shell relative to those used for the HB-3 nozzle weld are (Table 5.3 of Ref. 2)

$$\frac{0.850 \times 16.82}{0.850(16.82 + 4.24) + 8.5} = 0.54$$

Based on the information in Table G.3 of Ref. 2, this factor on stress is equal to a factor of $\sim 3 \times 10^{-2}$ on $P(F|E)$.

The value of $P(F|E)$ for the beltline region relative to that for the HB-3 nozzle weld (Ref. 7 analysis) is

$$100 (\text{area factor}) \times 0.3 (\Delta\dot{N}DTT \text{ factor}) \times 3 \times 10^{-2} (\text{stress factor}) = 0.9$$

C.7 Total value of $P(F|E)$

The total value of $P(F|E)$, considering the gamma effect on spatial distribution of damage, relative to that calculated in Ref. 7, which did not include the gamma effect, is

$$0.5 \text{ (HB-1, 2, 3, 4 nozzle welds)} + 0.9 \text{ (beltline)} = 1.4$$

C.8 Concluding remarks

Consideration of gamma effects on the spatial distribution of radiation damage and inclusion of some other refinements in the analysis resulted in a calculated probability of failure 40% greater than reported in Ref. 7. In terms of the precision of limiting values of the probability of failure, this difference is negligible.

There are at least two conservatisms in the analyses that deserve comment. The flaws in the nozzle "welds" extend out of the welds as initial flaws, if they are large enough (the probabilistic analysis includes a spectrum of flaw sizes), and once a flaw begins to propagate, if not already extending out of the weld, it must extend out of the weld in order to penetrate the wall. The portion of the flaw outside the weld is exposed to much lower stress than assumed. Thus, the potential for through-wall propagation of flaws in the nozzle welds is less than calculated.

In the beltline region, the situation is different because away from the nozzles there are no significant gradients in stress. However, the other conservatism that deserves comment pertains to flaw density. It is generally recognized that the flaw density in the shell material is substantially less than in the welds. However, it has been assumed in this analysis that the flaw density in the shell is the same as that in the welds. Based on that assumption, the beltline shell contributed $0.9/1.4 = 64\%$. If a more realistic flaw density of 0.1 times that for the welds were used, $P(F|E)_T$ would be only 0.6 times that in Ref. 7, and the shell would contribute only 17%.

In calculating the contribution of the beltline shell, it was assumed that RT_{NDT} for the shell material was the same as that for the nozzle welds. As indicated in Table D.21 of Ref. 2, A212B(LS) has a value of -10°F compared to $+10^\circ\text{F}$ for the nozzle weld. The LS orientation of the CVN specimens corresponds to an axially-oriented flaw propagating radially through the wall, which is the situation considered in the analysis. Thus, the beltline shell would actually contribute less to $P(F|E)_T$ than indicated.

The seam weld in the beltline shell region and the girth weld below the core were not included as separate entities. The seam weld has about the same surface area as that for the nozzle welds but has a very low transverse residual stress compared to the longitudinal stress in the nozzle welds, and there are no bending stresses. The stresses are somewhat higher than the shell stresses, but the area is much smaller than the shell area.

The girth-weld area is about four times that of the nozzle welds, but the total dpa is relatively low (7 in. beyond end of fuel), and the longitudinal residual stress is less because of less constraint. Furthermore, as mentioned above, an axially-oriented flaw has to extend out of the weld to extend through the wall, and thus the deeper the flaw the more propagation is governed by the nominal shell stresses. Eliminating the girth weld from detailed evaluation for this reason is a less conservative approach than used for the nozzle welds but is nevertheless realistic.

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Fig. C.1. Cross section of HFIR core/vessel at horizontal midplane (HMP) of core, showing locations of vessel beam-tube nozzles (HB-1, 2, 3, 4) and surveillance specimens (Keys 1, 2, 3, 4, 5, 6, 7). Keys 1, 2, 3, 4, 6, and 7 are centered on the HMP, while Key 5 is below the HMP. Keys 1, 2, 3, and 4 consist of circular arrays of specimens around the beam tubes (see Figs. C.2 and C.3).

NOZZLE	X, IN.	R _n , IN.	R _w , IN.	W, IN.	γ dpa / n dpa
HB-1,4	~0.5	5	10	1.375	1.4
HB-2	~0.5	7	15	1.375	1.1
HB-3	~0.5	5	10	1.375	1.3 (ESTIMATED)

Fig. C.2. Spatial relationship of surveillance specimens, flaws, and nozzle welds for Key-1 (HB-1), Key-2 (HB-2), Key-3 (HB-3), and Key-4 (HB-4). Keys consist of circular arrays of capsules, each containing three CVN specimens, concentric with nozzle bore.

Fig. C.3. Developed view of inner surface of HFIR vessel showing surveillance-specimen-capsule locations (positions) fast-neutron fluxes ($E > 1 \text{ MeV}$), and location of specimens pulled in 1983 and 1986 (see Figs. C.1 and C.2).

Fig. C.4. Neutron- and gamma-induced fission rate in ^{237}Np , as obtained from 1-D coupled neutron/gamma transport calculations, vs radial distance from vertical axes of HFIR core (Ref. 21).

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