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HFIR Vessel Life Extension with Enlarged HB-2 and HB-4 Beam Tubes

R. D. Cheverton
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**HFIR VESSEL LIFE EXTENSION WITH
ENLARGED HB-2 AND HB-4 BEAM TUBES**

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PREFACE

The proposed HFIR upgrade includes enlargement of the HB-2 and HB-4 beam tubes. The enlargements increase the neutron and gamma dose rates to the beam-tube nozzles in the vessel wall, and this tends to reduce the life of the vessel. A preliminary evaluation of the effect on vessel life was issued as a letter report (R. D. Cheverton and T. L. Dickson, *HFIR Vessel Life Extension with Enlarged HB-2 and HB-4 Beam Tubes*, RDC/RRD/LTR-98-1, May 28, 1998). It was intended that the "final" report would simply provide greater detail than the letter report. In the interim, however, substantial changes were made in the calculational models and in certain criteria. This explains the existence of significant differences in the results, and, of course, the results in this report supersede those in the preliminary report.

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NOMENCLATURE

a	flaw characteristic size
$\dot{a}$	rate of flaw growth
A	area of vessel region
A	constant in K_{Ic} equation
B	constant in K_{Ic} equation
$B(a)$	probability of non-detection
C	constant in K_{Ic} equation
c_b	flaw characterization factor (bending stress)
c_c	flaw characterization factor (cladding effect)
c_m	flaw characterization factor (membrane stress)
c_r	flaw characterization factor (residual stress)
dpa	displacements per atom per unit time
dpa	displacements per atom
e	uncertainty factor for ΔRT_{NDT}
f	fraction of total vessel-region area
$f(a)$	flaw-size distribution function
$F(a/r_i)$	flaw characterization factor (nozzle-corner flaw)
$F(a/r_n)$	crack characterization factor (nozzle-corner flaw)
K_I	stress intensity factor
K_{I_T}	total stress intensity factor
$K_{I(E/F)}$	stress intensity factor (emergency/faulted conditions)
$K_{I(HT)}$	stress intensity factor (hydro test)
K_{Ic}	static fracture toughness
$K_{Ic(E/F)}$	static fracture toughness (time of successive hydro test)
$K_{Ic(HT)}$	static fracture toughness (time of hydro test)
K_{JR}	ASME lower bound dynamic/arrest fracture toughness
K_j	stress intensity factor derived from j integral
N	flaw surface density

p	pressure
$p(E/F)$	pressure (emergency/faulted condition)
$p(F/E)_T$	total value of $P(E/F)$ for all vessel regions
$p(HT)$	pressure (hydro test)
$p(HT)_{max}$	pressure (maximum, hydro test)
$P(F/E)$	conditional probability of failure
$\bar{P}(F/E)$	conditional probability of failure (mean value)
r_c	nozzle-corner radius
r_F	radius of nozzle-corner part-circular flaw
r_i	radius of nozzle bore
r_n	radius, from center line of nozzle, defining location of nozzle-corner flaw
r_v	radius, vessel
RT_{NDT}	nil ductility reference temperature
RT_{NDT_0}	nil ductility reference temperature (initial value)
t	time
$t(HT)$	time of hydro test
T	temperature
T_v	temperature of vessel
$T_v(HT)$	temperature of vessel during hydro test
$T_v(E/F)$	minimum temperature of vessel during Δt (E/F condition)
w	vessel wall thickness
W	width of weld at outer surface of vessel
$\bar{W}$	average width of weld
$\Delta NDTT$	increase in nil ductility transition temperature
ΔRT_{NDT}	increase in nil ductility reference temperature
Δa	increase in flaw depth during Δt
Δt	time between hydro tests
Δt_{min}	time between successive hydro tests (minimum value)
σ	stress
σ	one standard deviation

σ_b	stress (bending)
σ_c	stress in cladding
σ_h	hoop stress
σ_m	stress (membrane)
σ_r	stress (residual)
$\phi(E)$	frequency of event
$\phi(F)$	frequency of failure
$\bar{\phi}(F)$	frequency of failure (mean value)
λ	attenuation constant for dpa

ABSTRACT

Proposed upgrades of HFIR experimental facilities include increasing the diameters of the HB-2 and HB-4 beam tubes. This increases the rate of radiation-induced embrittlement of the corresponding vessel nozzles and closely surrounding materials and thus increases the chances of hypothetical flaws propagating and possibly leading to vessel failure. To help justify the upgrade proposal, it is necessary to demonstrate analytically that the HFIR pressure vessel can be operated safely to year 2035.

Hydrostatic proof testing is and will continue to be used, periodically, to demonstrate the integrity of the vessel, but the frequency of testing and the probability of vessel failure during the tests must satisfy appropriate criteria. Updated probabilistic fracture mechanics evaluations, including (1) three-dimensional transport calculations of neutron and gamma dpa rates in the vessel and associated surveillance specimens, (2) three-dimensional stress and fracture-mechanics calculations for the vessel nozzles, and (3) upgrades in damage correlations and criteria, have been performed and indicate that with the proposed facility upgrade implemented in early 2000 the vessel useful life will be 50 effective full-power years, based on full power equal 100 MW. This corresponds to about 2035.

HFIR VESSEL LIFE EXTENSION WITH ENLARGED HB-2 AND HB-4 BEAM TUBES

1. Introduction

The potential for possible flaws (sharp, crack-like defects) in the HFIR pressure vessel to experience unstable growth increases with increasing exposure of the vessel to neutron and gamma radiation. The present exposure rates permit the vessel to operate for about 50 EFPY(100 MW)^a without excessive calculated probabilities of failure,^{1,2} which is a life extension from the present date [$\sim$ 21 EFPY (100 MW)] of about 30 EFPY(100 MW). The HFIR upgrade effort,³ however, includes larger-diameter beam tubes for HB-2 and HB-4, and these changes tend to result in higher exposure rates in the vicinity of the beam-tube nozzles by reducing the amount of water between the beam tube and the nozzle (Figs. 1 and 2). A preliminary evaluation⁴ of the increases indicated that they were substantial and would require reductions in conservatism used in previous life-extension studies in order to achieve 50 EFPY(100 MW). Consistent with this approach, more detailed stress, neutron- and gamma-flux, and deterministic and probabilistic fracture mechanics analytical models have been developed and applied, and the results indicate that 50 EFPY(100 MW) can be achieved with the enlarged beam tubes installed.

2. Approach to Establishing Vessel Integrity

It was apparent by 1986 that some areas of the HFIR vessel could not literally satisfy the ASME Code⁵ over the 10-y life-extension period desired at that time. The "intent" of the Code, however, could be satisfied by periodically subjecting the vessel to hydrostatic proof tests (hydro tests) that resulted in acceptable calculated probabilities of vessel failure for the hydro test as well as the credible worst-case operating condition, taking into account vessel degradation (radiation damage and stable flaw growth) during the time between hydro tests. This approach to establishing vessel integrity was proposed⁵ and accepted⁶ in 1988 and has been applied since restart in 1989.

The hydro test is conducted without fuel in the core, and thus failure of the vessel during the hydro test is not a safety issue with regard to release of activity. Even so, the probability of failure should be reasonably low, and probabilistic fracture mechanics calculations were made to determine what the probability is.^{2,5}

A successful hydro test (no vessel failure) demonstrates with a very high degree of confidence that the vessel is safe to operate, when exposed to the worst-case operating conditions, until the time of the next hydro test. Even so, as a defense in-depth measure, a probabilistic fracture mechanics analysis was performed

^aEffective full-power years based on full power = 100 MW.

considering the worst-case operating conditions, and related criteria proposed by the Nuclear Regulatory Commission (NRC) for commercial power reactors were used as acceptance criteria. Thus, the permissible life extension of the HFIR vessel is judged on the basis of (1) the calculated probabilities of vessel failure for the hydro test and credible worst-case operating conditions and (2) being able to establish reasonable and adequate hydro-test conditions (pressure, temperature, frequency), which, as indicated, are input to the probabilistic analyses.

3. Hydrostatic Proof Test Conditions

As indicated in Sect. 2, the hydro-test conditions were selected such that the potential for vessel failure during the hydro test was greater than that for the credible worst-case operating condition during the time between successive hydro tests. The longer the time between hydro tests, the greater the radiation-induced reduction in fracture toughness, the greater the extent of stable-crack growth, and, thus, the greater the minimum permissible hydro-test pressure. And, the greater the hydro-test pressure, the greater the probability of vessel failure during the hydro test. A lower limit on the hydro-test pressure was specified by a Department of Energy (DOE) review committee as 1.25 times the design pressure,⁶ which, under the present operating conditions, is the high-pressure primary-system nominal rupture-disc setting (650 psi). Thus, the lower limit is 813 psi, and thus far there has been no reason to challenge this value.

The hydro-test pressure specified for the present operating conditions is 900 psid,^{5,7} and the same test pressure has been specified for demonstrating the integrity of some of the welds in the primary-system piping. Thus, 900 psi has been used as a first trial in the probability calculations and in the calculation of the maximum permissible time between hydro tests (Δt). Because the desired life extension can be achieved with this pressure, no other hydro-test pressures were considered.

The vessel temperature specified for the hydro test (85°F) is a convenient test value. There is no need for the hydro-test temperature to be less than the temperature selected for the worst-case operating condition because, as explained in Sect. 2 and Appendix A, a difference in temperature is accommodated by the other two test conditions (pressure and frequency).

The time between hydro tests is also a factor in calculating the probability of vessel failure, when the success of a hydro test (no vessel failure) is accounted for in the analysis. Reducing the time between tests results in a lower calculated probability, and, thus, Δt may be dictated by this consideration rather than the relative potential of failure for the hydro test and worst-case operating condition.

The worst-case operating conditions specified for calculation of hydro-test conditions were a vessel differential pressure of 679 psi (corresponds to the maximum rupture-disc burst pressure) and a vessel temperature of 80°F (the minimum temperature expected for the coolant in the cooling-tower basin).

The potential for vessel failure can be defined in both a deterministic and a probabilistic manner for the purpose of determining hydro-test conditions. The deterministic approach, which was used for all previous calculations of hydro-test conditions,^{1,5,7,8} tends to be very conservative. The degree of conservatism has not resulted in unacceptable hydro-test conditions for the present design. However, application of the deterministic approach to the upgrade design (enlarged HB-2 and HB-4 beam tubes) results in maximum permissible times between hydro tests that are substantially less than desired. Thus, the probabilistic approach, which is less conservative but which adequately accounts for uncertainties, has been applied to the upgrade design. Based on the expectation that the upgrade will be implemented at 22.6 EFPY(100 MW),⁹ results of the analysis (Appendix A) indicate a maximum permissible time between hydro tests in excess of the value specified for the present design [2.6 EFPY(100 MW)]. However, in the calculation of the probability of vessel failure (Sect. 4), the time between hydro tests must be no greater than 3.0 EFPY(100 MW) until 40 EFPY(100 MW) in order that the proposed limit on the calculated conditional probability of failure be satisfied. Furthermore, because the HB-2 nozzle damage rates for the upgrade design are so much greater than for the present design, it is recommended that the first time increment, after the upgrade is accomplished, be limited to 1.0 EFPY(100 MW) and the second to 2.0 EFPY(100 MW), as an added precaution. Thereafter, 3.0 EFPY(100 MW) is acceptable.

4. Probabilistic Fracture Mechanics Evaluation

4.1 Methodology

Details of the methodology are discussed in Refs. 2 and 10. Sufficient information is provided here for a basic understanding.

The methodology was developed at ORNL for the NRC for application to commercial water-cooled power reactors^{11,12,13} and is endorsed by the NRC for that application.¹⁴ A product of the ORNL development effort is the probabilistic fracture mechanics code FAVOR,¹² which calculates the conditional probability of failure $[P(F/E)]$ using Monte Carlo techniques. Many vessels are simulated (typically $10^6 - 10^9$), using unique sets of values of the various parameters related to unstable growth (propagation) of flaws. A deterministic analysis is made for each of the simulated vessels to determine the ratio K_I/K_{Ic} , where K_I = stress intensity factor associated with flaws, and K_{Ic} = material fracture toughness, a material property. Vessel failure is assumed to occur if $K_I/K_{Ic} \geq 1$. The conditional probability of failure is equal to the number of vessels for which $K_I/K_{Ic} \geq 1$ divided by the number of vessels simulated. The frequency of failure $[\phi(F)]$ is obtained from

$$\phi(F) = \phi(E) P(F/E), \quad (1)$$

where $\phi(E)$ is the frequency of the event.

The stress intensity factor (K_I) represents the driving "force" for propagation of the flaw and can be obtained from

$$K_I = c \sigma \sqrt{\pi a}, \quad (2)$$

where a = flaw depth
 c = geometry factor
 σ = far-field stress

Values of K_I can be obtained for complex stress distributions and geometries using finite-element techniques, as described in Refs. 15, 16, 17, and 18. This was done for all flaws in the HFIR analyses, as discussed further in Sect. 4.3.6.

The fracture toughness (K_{Ic}) which represents the material's resistance to propagation of flaws, is obtained from

$$K_{Ic} = A + B \exp \left[C (T - RT_{NDT}) \right], \quad (3)$$

where

$$RT_{NDT} = RT_{NDT_0} + \Delta RT_{NDT} \quad (4)$$

= reference nil-ductility transition temperature
(a measure of the fracture toughness)

RT_{NDT_0} = initial (zero dose) value of RT_{NDT}

ΔRT_{NDT} = increase in RT_{NDT} due to irradiation

A, B, C = constants

T = temperature

Values of A , B , and C for the ASME lower-bound curve are available in the ASME Code¹⁹ for nuclear reactor pressure vessel materials (A and B were multiplied by 1.43 to obtain the mean curve used in the probabilistic analysis).¹¹ RT_{NDT_0} was determined from tests on HFIR vessel materials;⁵ and ΔRT_{NDT} is obtained from the HFIR vessel surveillance program.^{5,7}

Aside from the study discussed in this report, the most recent application of the probabilistic fracture mechanics methodology to the HFIR vessel is discussed in Ref. 2. Modifications have been made to the Ref. 2 model and input in an effort to reduce the conservatism and thus obtain a more realistic result. To this same end, the benefit of periodic hydro testing in reducing the probability of failure is also included. This latter feature and the modifications are discussed briefly in the following subsections.

4.2 Benefit of hydrostatic proof testing

The above methodology for calculating $P(F/E)$ does not directly account for the benefit of hydro testing. However, because of the need perceived in connection with the desire to increase the size of the HB-2 beam tube, a method was developed for doing so specifically for HFIR.¹⁰

Immediately following a successful hydro test, the probability of failure at hydro-test conditions is zero. In the calculation of $P(F/E)$, this eliminates from the population of simulated vessels all of those for which failure was calculated. As time progresses, the calculation will identify additional failures because of continuing degradation, but the total number of failed vessels will be less by the number eliminated by the hydro test. The next hydro test, if successful, will once again reduce the probability to zero, and another set of vessels will be eliminated from the population.

Because the hydro test is a more severe loading condition than the worst-case $(E/F)^b$ operating condition, in a sense the probability of failure for the E/F condition following a successful hydro test is less than zero. As time progresses, $P(F/E)(E/F)$ increases but may remain less than zero up to the time of the next hydro test. To satisfy the purpose of hydro testing, it is only necessary that $P(F/E)(HT, t = t_i) \geq P(F/E)(E/F, t = t_i + \Delta t)$, where t_i is the time of a hydro test and Δt is the time between hydro tests.

The reduction in $P(F/E)$ due to hydro testing is a function of the shape (slope) of the $P(F/E)$ vs t uncorrected curve and the time between hydro tests (Δt). As mentioned in Sect. 3, decreasing Δt reduces $P(F/E)_{max}$, and thus satisfying a maximum permissible value of $P(F/E)$ and/or $\bar{\phi}(F)$ rather than satisfying the above hydro-test-related condition may dictate the values of Δt .

4.3 Model and criteria upgrades

4.3.1 Regions of vessel

The vessel is divided into regions to accommodate surface "gradients" in materials (nozzle material, nozzle weld, shell material, shell seam welds, etc.), neutron and gamma fluxes, stresses, flaw density, and surface area. Each region has a unique combination of "values" of these parameters, and a value of $P(F/E)$ is calculated for each. The total value of $P(F/E)$ calculated for the vessel is

$$P(F/E)_T = \sum_i N_i A_i \hat{P}(F/E)_i, \quad (5)$$

where

$$\begin{aligned} \hat{P}(F/E)_i &= P(F/E) \text{ for the } i^{\text{th}} \text{ region with a single flaw} \\ A_i &= \text{surface area of } i^{\text{th}} \text{ region} \\ N_i &= \text{flaw surface density for } i^{\text{th}} \text{ region.} \end{aligned}$$

Thus, except for values of $N_i A_i$ much greater than unity, $P(F/E)_i$ is directly proportional to the area and flaw density of the region and thus to the number of flaws in the region. (As mentioned in Ref. 12, the actual calculation of $P(F/E)$ is performed in such a way that "any" value of $N_i A_i$ can be used and an accurate result obtained.)

^bUsing terminology in the ASME Code, the worst-case condition is referred to as the emergency/faulted (E/F) condition.

In Ref. 2, the vessel was divided into three regions. For the present study, 31 regions are used. These regions are described in Appendix C.

4.3.2 Flaw density

As indicated above, the flaw density (N_i) in Eq. (5) is the flaw density following the final fabrication-stage inspection and repair operation. The value used in previous HFIR probabilistic studies^{2,5,8} was the value mentioned by the NRC in Ref. 14 and used in PWR studies¹³ as the density prior to the final fabrication-stage inspection and repair operation. As shown in Appendix D,

$$\frac{N_i \text{ (post-inspection and repair)}}{N_i \text{ (pre-inspection and repair)}} \approx 0.6$$

Thus, in effect, for HFIR, an uncertainty factor of $0.6^{-1} = 1.7$ is applied to the value of N_i used in the referenced PWR studies. The latter value is based on 1 flaw/m³ of weld material and in PWR studies has been used for both welds and base material. However, base material tends to have a much lower flaw density, and the NRC has included 0.1 flaws/m³ for base material in studies at ORNL.²⁰ For the present HFIR studies, a best-estimate volumetric flaw density of 1 flaw/m³ was used for the welds, and 0.1 flaws/m³ was used for the base material. Flaw surface density was obtained by multiplying the volumetric density by the nominal wall thickness (3.0 in.).

Flaws can also exist in the weld-deposit cladding. However, because of the relatively high fracture toughness of the cladding and the small size of flaws that could reside in the cladding, the potential for propagation is very small.²¹ It was assumed that for flaws in the cladding large enough to propagate the flaw density was the same as for the underlying material, and that flaws in the cladding were simply an extension of flaws in the underlying material. This was consistent with the assumption mentioned below that all flaws were surface-breaking flaws (Fig. 3 in Ref. 2).

For the HFIR studies, it was assumed that all of the postulated flaws in the weld and base material were inner-surface flaws normal to the surface and to the direction of the principal stresses. This is a very conservative assumption because inspection of vessel materials shows that most of the flaws are embedded, and embedded flaws have a much lower potential for propagating. Furthermore, for a surface flaw to propagate through the wall of the vessel, it must extend in length. It is assumed that extension will take place on the surface, but in reality the relatively high toughness of the cladding restricts flaw-length extension to the base material, in which case the extended portion of the flaw is beneath the cladding and thus not exposed to the surface; that is, the extended portion of the flaw is embedded.²² Although the entire length of the flaw is not embedded, the potential for through-wall propagation is less than that for the assumed surface flaw.

4.3.3 Flaw-size-distribution function

The flaw-size-distribution function used in the present analysis and its truncation flaw size (1.5 in.) are the same as those used in previous HFIR analyses.^{2,5} The number

of average flaw sizes, however, is greater than before (15 compared to 8), and thus the numerical representation of the flaw-size-distribution function is different. The new flaw sizes and relative number of flaws for each flaw size are included in Appendix D. This change was made to improve the accuracy of the numerical analysis.

4.3.4 Radiation-damage correlation

Remec, et al.,²³ have determined that gamma radiation contributes significantly to the degradation of the HFIR vessel material, and that the increase in the nil ductility transition temperature ($\Delta NDTT$) correlates well with the total (neutron plus gamma) dpa, as indicated in Fig. 3. Gammas were included in Ref. 2, but it was assumed that $\Delta NDTT$ was linear with dpa, as indicated in Ref. 7. A comparison of the two correlations is indicated in Fig. 4, and it is apparent that extrapolation with the linear correlation is excessively conservative for $dpa > 3 \times 10^{-3}$. For the present beam tubes and a maximum vessel life of 50 EFPY(100 MW), the degree of conservatism is not excessive. However, for the proposed enlarged HB-2 beam tube, which results in $dpa \approx 5 \times 10^{-2}$ at 50 EFPY(100 MW), the linear extrapolation is not satisfactory. Thus, the Remec, et al., correlation, as depicted in Figs. 3 and 4, was used for the present study.

4.3.5 Neutron and gamma fluxes and dpa rates

Preliminary studies⁴ of the effect of increasing the size of the HB-2 beam tube indicated that HB-2 would dominate $P(F/E)$. Thus, the initial effort in refining the model for calculating dpa rates was centered on HB-2, which, fortunately, is nearly axisymmetric, permitting a two-dimensional analysis. An 84-group cross-section set was developed; the most recent proposed design was incorporated in the model; and calculations of total $(n+\gamma)$ dpa were made for both the present design and the proposed new design.²⁴ Typical iso dpa plots are included in Appendix E. These results indicate that the new design results in a factor of 10 increase in dpa at the nozzle corner but only a 50% increase at the nozzle weld. These increases are substantially less than reported in Ref. 4. A thorough investigation of the two evaluations indicates that the most recent is correct.

Neutron and gamma flux calculations were also made for HB-1, HB-3, and HB-4, which required three-dimensional models because of lack of symmetry. Corresponding iso dpa plots are included in Appendix E.

The above calculated dpa rates were normalized to HFIR dosimetry data before using the dpa rates in the calculation of hydro-test conditions and in the $P(F/E)$ analysis. Dosimetry data are available for HB-2 and HB-4,²³ and the data for HB-4 are appropriate for HB-1 because the two designs are mirror images. The normalization factors for the new designs of HB-2 and HB-4 were assumed to be the same as for the present designs, and the average of the factors for HB-2 and HB-4 were used for HB-3. The factors are included in Appendix E.

4.3.6 Stresses and stress intensity factors

Nozzles introduce elevated stresses that enhance the potential for propagation of flaws. Estimates of these stresses were included in Ref. 5 and more recent studies. In preparation for this study, more detailed and up-to-date, three-dimensional, finite-element calculations were made of stress distributions in and around HB-2 and HB-3.²⁵ These stresses, which were used in calculating stress intensity factors for hypothetical flaws in corresponding regions, are discussed in Appendix F.

For all of the HFIR vessel studies beginning with Ref. 5, and including the present study, stress intensity factors (K_I) for all regions but the nozzle corners were calculated using finite-element-analyses-derived influence coefficients,^{15,16} calculated for PWR vessels,^{26,27,28} and the above stresses calculated for the HFIR vessel. These K_I values included the contribution of the stainless-steel inner-surface cladding, which is due to the differential thermal expansion between cladding and base material.

For all studies but the present, stress intensity factors for the nozzle corners were calculated using Eq. (2), with geometry factors (c) obtained from finite-element analyses.^{5,29} These calculations did not directly account for the variation of K_I along the flaw front, nor did they include the contribution of the cladding. These omissions were not of concern because for the present design (non-enlarged HB-2 and HB-4), the high initial toughness of the nozzle material rendered the nozzle-corner contribution negligible. For the upgrade design, however, the relatively high dose rate at the HB-2 nozzle corner makes the HB-2 nozzle corner dominant. Thus, a more refined derivation and application of the nozzle corner K_I values was necessary. This was done by performing a three-dimensional finite-element fracture-mechanics analysis for the HB-2 nozzle corner with the cladding included.¹⁸ A discussion of the Ref. 18 analysis and application of the results are included in Appendix G.

4.3.7 Events and their frequencies of occurrence

There are two events that must be considered: hydrostatic proof test and the credible worst-case operating (E/F) condition, which is defined as the pressure reaching the rupture-disc failure value with the vessel at 80°F. The specified time between hydro tests at present is 2.6 EFPY(100 MW). With the enlarged HB-2 beam tube installed, the time between tests will be reduced to ~ 1.0 EFPY(100 MW) for the first test period after the design change and 2.0 EFPY(100 MW) for the second period. Thereafter, it will be 3.0 EFPY(100 MW). Thus, a reasonable frequency of hydro testing to use for calculating the frequency of failure is 0.33 y^{-1} .

The best-estimate frequency of occurrence of the E/F condition was reported in Ref. 8 to be $7 \times 10^{-5} \text{ y}^{-1}$, and a very conservative value of $1 \times 10^{-2} \text{ y}^{-1}$ was used in Refs. 1, 2, and 8 for estimating the mean frequency of failure. A recent reevaluation³⁰ resulted in a mean value of $2 \times 10^{-5} \text{ y}^{-1}$, which was used in this study for the calculation of the mean frequency of failure for the E/F condition.

4.3.8 Criteria for limiting values of $P(F/E)(HT)$, $\bar{\phi}(F)(HT)$, $P(F/E)(E/F)$, and $\bar{\phi}(F)(E/F)$

The NRC has specified a "safety goal" of 1×10^{-4} core melts/reactor year due to all causes³¹ and a "safety goal" of 5×10^{-6} vessel failures/reactor year,¹⁴ the latter of which is referred to as a mean value. In Refs. 1 and 8, the former criterion was used for the HFIR hydro-test $\bar{\phi}(F)$ limit, and the latter value was used for the E/F -condition $\bar{\phi}(F)$ limit. Because fuel does not reside in the HFIR vessel during hydro testing, there is no concern over fuel melting in the event of vessel failure. Thus, it is suggested that the HFIR limit for $\bar{\phi}(F)(HT)$ be increased to 1×10^{-3} failures/year.

The NRC "limit" of $5 \times 10^{-6} \text{y}^{-1}$ for vessel failure during operation is based on the consequences of failure, which are much greater for a PWR power plant than for HFIR. Thus, a larger limit for HFIR would be appropriate. Even so, this is not being proposed at this time.

The concept of frequency of failure $[\phi(F)]$ has meaning when applied to a large number of real vessels, such as the commercial PWR vessels (how many vessels will fail over a period of time?). However, the physical meaning of "frequency of failure" is not so clear when applied to a single vessel, particularly when the frequency of the event is large compared to the inverse of the design life. The "chance of failure," when the event takes place $[P(F/E)]$, tends to be a more appropriate parameter, and a best-estimate value that has been accepted previously by ORNL/RRD and DOE for HFIR core damage is 1×10^{-6} (Ref. 32).

It is proposed that $P(F/E)$ be accepted as a probabilistic parameter for judging the potential for vessel failure during hydro testing, and that 1×10^{-5} be specified as the maximum permissible value. The higher maximum permissible value is justified because fuel does not reside in the vessel during the test.

The use of $\phi(F)$ is physically more meaningful for the E/F condition because of the very low frequency of occurrence, but $P(F/E)$ should still be used as a probabilistic parameter for judging the potential for failure, and it is proposed that 1×10^{-6} be specified as the limit of the best-estimate value.

4.3.9 Summary of input for $P(F|E)$ analysis

A summary of selected input for calculation of $P(F/E)$ is provided in Table 1 for HB-2, Table 2 for HB-3, Table 3 for HB-4, Table 4 for HB-1, and Table 5 for the vessel shell and its two welds (seam weld and circumferential weld below the core). The hydro-test and E/F -conditions input are $p(HT) = 900$ psi, $T_v(HT) = 85^\circ\text{F}$, $p(E/F) = 679$ psi, and $T_v(E/F) = 80^\circ\text{F}$. The relation $\Delta NDTT = f(dpa)$ is that given in Appendix E as Eq. (E.1). The parameter dpa is simulated and has a standard deviation of $\pm 20\%$. Other parameters simulated in the Monte Carlo-type $P(F/E)$ analysis and additional input are discussed in Ref. 2.

The values of A , B , and C included in Ref. 2 for the fracture-toughness (K_{Ic}) equation [Eq. (3)] are for plane-strain (full constraint) conditions. Because of the geometry of the nozzle, full constraint does not exist for a nozzle-corner flaw, and thus the actual

Table 1. Summary of selected input for HB-2 P(F|E) analysis

Parameter	Region 1 (Nozzle Corner)	Region 3 (Nozzle Corner)	Region 2 (Nozzle Corner)
$RT_{NDT_0}, ^\circ F$	-110	-110	-110
dpa	$2.74 \times 10^{-3} + 1.25 \times 10^{-3} (t - 22.6), t \geq 22.6, t \text{ in EFPY}(100 \text{ MW})$		
σ_p, ksi	$16p$	$12p$	$8p$
$K_p, \text{ksi} \sqrt{\text{in.}}$	$\left[F(a/r_i)_p \sigma_p + F(a/r_i)_c \sigma_c \right] \sqrt{\pi a}$		
$F(a/r_i)_p$	$3.24 - 2.97(a/r_i)$		1.0
$F(a/r_i)_c$	$0.420 - 2.01(a/r_i) + 4.18(a/r_i)^2$		
a/r_i	$0.143 a, a \text{ in inches}$		
A, ft^2	0.31	0.21	0.31
$N, \text{flaws/ft}^2$	7×10^{-4}	7×10^{-4}	7×10^{-4}
Parameter	Region 4 (Nozzle Annulus)	Region 5 (Nozzle Annulus)	
$RT_{NDT_0}, ^\circ F$	-110	-110	
$dpa(t,a)$	$1.98 \times 10^{-3} e^{-0.22a} + 3.87 \times 10^{-4} (t - 22.6)$		
σ, ksi	$5(6-a)p$	$2.5(6-a)p$	
K_i	6:1 semiellipse, axial, cladding included	6:1 semiellipse, circumferential, cladding included	
A, ft^2	1.5	1.5	
$N, \text{flaws/ft}^2$	7×10^{-4}	7×10^{-4}	
Parameter	Region 6 (Nozzle Weld)	Region 7 (Nozzle Weld)	
$RT_{NDT_0}, ^\circ F$	10	10	
$dpa(t,a)$	$1.51 \times 10^{-3} e^{-0.50a} + 1.17 \times 10^{-4} (t-22.6) e^{-0.19a}$		
σ, ksi	$p[17.5 + 5.9 (1-2 a/w)] + 8.5$	$p[8.1 - 3.9 (1-2a/w)] + 8.5$	
K_i	6:1 semiellipse, axial, cladding included	6:1 semiellipse, circumferential, cladding included	
A, ft^2	0.32	0.14	
$N, \text{flaws/ft}^2$	7×10^{-3}	7×10^{-3}	

Table 2. Summary of selected input for HB-3 P(F|E) analysis

Parameter	Region 8 (Nozzle Corner)	Region 10 (Nozzle Corner)	Region 9 (Nozzle Corner)
$RT_{NDT_0}, ^\circ F$	-78	-78	-78
dpa(t)	$1.78 \times 10^{-4}t$, t in EFPY(100 MW)		
σ_p , ksi	16p	12p	8p
K_p , ksi $\sqrt{in.}$	$\left[F(a/r_i)_p \sigma_p + F(a/r_i)_c \sigma_c \right] \sqrt{\pi a}$		
$F(a/r)_p$	$3.24 - 2.97(a/r_i)$		1.0
$F(a/r)_c$	$0.420 - 2.01(a/r_i) + 4.18(a/r_i)^2$		
a/r_i	0.250 a, a in inches		
A, ft ²	0.10	0.07	0.10
N, flaws/ft ²	7×10^{-4}	7×10^{-4}	7×10^{-4}
Parameter	Region 11 (Nozzle Annulus)	Region 12 (Nozzle Annulus)	
$RT_{NDT_0}, ^\circ F$	-78	-78	
dpa(t)	$1.05 \times 10^{-4}t$		
σ , ksi	$5(6-a)p$	$2.5(6-a)p$	
K_i	6:1 semiellipse, axial, cladding included	6:1 semiellipse, circumferential, cladding included	
A, ft ²	0.78	0.78	
N, flaws/ft ²	7×10^{-4}	7×10^{-4}	
Parameter	Region 13 (Nozzle Weld)	Region 14 (Nozzle Weld)	
$RT_{NDT_0}, ^\circ F$	10	10	
dpa(t,a)	$7.3 \times 10^{-5} e^{-0.39at}$		
σ , ksi	$p[21.4 + 1.8(1-2a/w)] + 8.5$	$p[8.3 - 2.5(1-2a/w)] + 8.5$	
K_i	6:1 semiellipse, axial, cladding included	6:1 semiellipse, circumferential, cladding included	
A, ft ²	0.21	0.09	
N, flaws/ft ²	7×10^{-3}	7×10^{-3}	

Table 3. Summary of selected input for HB-4 P(F|E) analysis

Parameter	Region 15 (Nozzle Corner)	Region 17 (Nozzle Corner)	Region 16 (Nozzle Corner)
$RT_{NDT_0}, ^\circ F$	-40	-40	-40
$dpa(t)$	$2.4 \times 10^{-3} + 2.8 \times 10^{-4} (t - 22.6), t \text{ in EFY}(100 \text{ MW})$		
σ_p, ksi	$16p$	$12p$	$8p$
$K_b, ksi \sqrt{in.}$	$\left[F(a/r_i)_p \sigma_p + F(a/r_i)_c \sigma_c \right] \sqrt{\pi a}$		
$F(a/r_i)_p$	$3.24 - 2.97(a/r_i)$		1.0
$F(a/r_i)_c$	$0.420 - 2.01 (a/r_i) + 4.18(a/r_i)^2$		
a/r_i	0.250 a, a in inches		
A, ft ²	0.10	0.07	0.10
N, flaws/ft ²	7×10^{-4}	7×10^{-4}	7×10^{-4}
Parameter	Region 18 (Nozzle Annulus)	Region 19 (Nozzle Annulus)	
$RT_{NDT_0}, ^\circ F$	-40	-40	
$dpa(t)$	$1.52 \times 10^{-3} + 1.15 \times 10^{-4} (t - 22.6)$		
σ, ksi	$5(6-a)p$	$2.5(6-a)p$	
K_I	6:1 semiellipse, axial, cladding included	6:1 semiellipse, circumferential, cladding included	
A, ft ²	0.78	0.78	
N, flaws/ft ²	7×10^{-4}	7×10^{-4}	
Parameter	Region 20 (Nozzle Weld)	Region 21 (Nozzle Weld)	
$RT_{NDT_0}, ^\circ F$	10	10	
$dpa(t,a)$	$1.19 \times 10^{-3} e^{-0.44a} + 5.0 \times 10^{-5} (t - 22.6) e^{-0.34a}$		
σ, ksi	$p[21.4 + 1.8 (1 - 2a/w)] + 8.5$	$p[8.3 - 2.5 (1 - 2a/w)] + 8.5$	
K_I	6:1 semiellipse, axial, cladding included	6:1 semiellipse, circumferential, cladding included	
A, ft ²	0.21	0.09	
N, flaws/ft ²	7×10^{-3}	7×10^{-3}	

Table 4. Summary of selected input for HB-1 P(F|E) analysis

Parameter	Region 22 (Nozzle Corner)	Region 24 (Nozzle Corner)	Region 23 (Nozzle Corner)
$RT_{NDT_o}, ^\circ F$	-40	-40	-40
dpa	$1.06 \times 10^{-4} t, t \text{ in EPFY}(100 \text{ MW})$		
σ_p, ksi	16p	12p	8p
$K_p, \text{ksi} \sqrt{\text{in.}}$	$\left[F(a/r)_p \sigma_p + F(a/r)_c \sigma_c \right] \sqrt{\pi a}$		
$F(a/r)_p$	3.24 - 2.97(a/r)		1.0
$F(a/r)_c$	$0.420 - 2.01(a/r) + 4.18(a/r)^2$		
a/r_i	0.250 a, a in inches		
A, ft ²	0.10	0.07	0.10
N, flaws/ft ²	7×10^{-4}	7×10^{-4}	7×10^{-4}
Parameter	Region 25 (Nozzle Annulus)	Region 26 (Nozzle Annulus)	
$RT_{NDT_o}, ^\circ F$	-40	-40	
dpa(t)	$6.7 \times 10^{-5} t$		
σ, ksi	5(6-a)p	2.5(6-a)p	
K_i	6:1 semiellipse, axial, cladding included	6:1 semiellipse, circumferential, cladding included	
A, ft ²	0.78	0.78	
N, flaws/ft ²	7×10^{-4}	7×10^{-4}	
Parameter	Region 27 (Nozzle Weld)	Region 28 (Nozzle Weld)	
$RT_{NDT_o}, ^\circ F$	10	10	
dpa(t,a)	$5.3 \times 10^{-5} e^{-0.44a} t$		
σ, ksi	$p[21.4 + 1.8(1-2a/w)] + 8.5$	$p[8.3 - 2.5(1-2a/w)] + 8.5$	
K_i	6:1 semiellipse, axial, cladding included	6:1 semiellipse, circumferential, cladding included	
A, ft ²	0.21	0.09	
N, flaws/ft ²	7×10^{-3}	7×10^{-3}	

Table 5. Summary of selected input for the vessel circumferential weld, vessel axial weld, and vessel shell P(F|E) analyses

Parameter	Region 29 (Vessel Circumferential Weld)	Region 30 (Vessel Axial Weld)	Region 31 (Vessel Shell)
$RT_{NDT_0}, ^\circ F$	60	60	-10
$dpa(t,a)$	$3.8 \times 10^{-5} e^{-0.62at}$	$5.4 \times 10^{-5} e^{-0.63at}$	$5.4 \times 10^{-5} e^{-0.63at}$
σ, ksi	$16p + 8.5$	$8p + 8.5$	$16p$
K_I	6:1 semiellipse, axial, cladding including	6:1 semiellipse, circumferential cladding included	6:1 semiellipse, axial, cladding included
A, ft^2	1.4	0.24	100
$N, \text{flaws}/ft^2$	7×10^{-3}	7×10^{-3}	7×10^{-4}

fracture toughness is somewhat higher than used in the analysis.³³ This constitutes an additional conservatism.

Based on analyses^{34,35} and measurements,³⁶ the differential-thermal-expansion-induced stress in the cladding (σ_c) at 85°F was specified as 25 ksi.

4.4 Results of probabilistic analysis

4.4.1 Beneficial effect of hydro testing not considered

The best-estimate calculated conditional probability of failure of the HFIR vessel (with the enlarged HB-2 and HB-4 beam tubes) for a hydro test at 50 EFPY(100 MW), without accounting for the beneficial effect of hydro testing, is 6.7×10^{-5} . The contribution by region of the vessel is given in Table 6, and the corresponding time history of the total $P(F/E)$ is given in Fig. 5. As indicated in Table 6, HB-2 accounts for 75% of the total, and the detailed results in Appendix I show that the HB-2 nozzle corner accounts for 55%. The vessel lower circumferential weld contributes only 3% and the vessel shell and axial weld less than 1%. Therefore, the area of the vessel of concern is very localized.

Table 6. Best-estimate $P(F/E)_i$ for hydro test at 50 EFPY(100 MW) without benefit of hydro testing considered

Region	$P(F/E)_i$
HB-1	3.2×10^{-6}
HB-2	5.0×10^{-5}
HB-3	4.5×10^{-6}
HB-4	7.9×10^{-6}
Cir weld	2.0×10^{-6}
Axial weld	$< 1 \times 10^{-7}$
Shell	$< 1 \times 10^{-7}$
Total	6.7×10^{-5}

To obtain a reasonable estimate of the mean value of $P(F/E)_T$, the best estimate is multiplied by the ratio of mean flaw density to best-estimate flaw density. A value of this ratio that has been used in PWR studies¹³ is 45. Thus, $\bar{P}(F/E)_T(HT, t = 50 \text{ EFPY}) = 6.7 \times 10^{-5} \times 45 = 3.0 \times 10^{-3}$.

After the first two hydro-test periods, the frequency of hydro testing is 0.33 y^{-1} . Thus,

$$\bar{\phi}(F)_J(HT, t = 50) = 3.0 \times 10^{-3} \times 0.33 = 1.0 \times 10^{-3} \text{ failures/year.}$$

This satisfies the criterion set forth in Sect. 4.3.8 for the calculated mean frequency of failure ($1 \times 10^{-3} \text{ y}^{-1}$), although the best-estimate value of $P(F/E)(HT, t = 50)$ does not satisfy the corresponding criterion (1×10^{-5}).

As discussed in Sect. 4.2, $P(F/E)$ can be reduced by including the effect of hydro testing in the calculation of $P(F/E)$. When this is done (Sect. 4.4.2), both criteria are satisfied.

For the E/F condition [$p(E/F) = 679 \text{ psid}$, $T_v(E/F) = 80^\circ\text{F}$] at 50 EFPY(100 MW),

$$P(F/E)_T = 1.7 \times 10^{-5},$$

and the mean frequency of the event is $2 \times 10^{-5} \text{ y}^{-1}$. Thus

$$\begin{aligned}\bar{\phi}(F)(E/F) &= 2 \times 10^{-5} \times 45 \times 1.7 \times 10^{-5} \\ &= 1.5 \times 10^{-8} \text{ y}^{-1}\end{aligned}$$

From Sect. 4.3.8, the maximum permissible value of $P(F/E)_T$ is 1×10^{-6} and for $\bar{\phi}(F)$ is $5 \times 10^{-6} \text{ y}^{-1}$. Thus, one criterion is satisfied, and the other is not.

The above probabilities for the E/F condition do not reflect the beneficial effect of hydro testing. When that effect is included (Sect. 4.4.2) both of the criteria are satisfied.

4.4.2 Beneficial effect of hydro testing included in calculation of $P(F/E)(HT)$

The beneficial effect of hydro testing in the calculation of $P(F/E)$ is determined in Appendix I and is illustrated in Fig. 6 for $P(F/E)(HT)$. In this figure, $P(F/E)_{\max,n}$ (lower "curve") is the maximum value of $P(F/E)$ during the time between hydro tests and occurs just prior to a hydro test. Immediately after the preceding hydro test, $P(F/E) = 0$, and as time progresses $P(F/E)$ increases and reaches a maximum value at the time of the next hydro test [see typical $P(F/E)$ vs t in Fig. 6]. Immediately following the hydro test, $P(F/E) = 0$ again, and a similar time history is repeated. Thus, the $P(F/E)_{\max}$ values in Fig. 6 are values of $P(F/E)$ just prior to a hydro test for a given set of Δt values. The existence of a maximum in the "curve" is the result of the saturation effect observed in the upper curve in Fig. 6 and the particular set of Δt values used.

As indicated by a comparison of the two "curves" in Fig. 6, including the beneficial effect of hydro testing in the calculation of $P(F/E)(HT)$ reduces the maximum value of $P(F/E)(HT)$ from 6.7×10^{-5} to 9.5×10^{-6} , assuming a vessel lifetime of 50 EFPY(100 MW), and this satisfies the proposed maximum permissible value of 1×10^{-5} . The corresponding mean frequency of failure, letting $\bar{\phi}(E) = 0.33$, is

$$0.33 \times 45 \times 9.5 \times 10^{-6} = 1.4 \times 10^{-4} \text{y}^{-1},$$

which satisfies the proposed maximum permissible value of 1×10^{-3} . Thus, both criteria are satisfied.

The particular values of $P(F/E)_{\max}$ were achieved by letting Δt for the first, second, and subsequent hydro-test periods be 1.0, 2.0, and 3.0 EFPY(100 MW), respectively. Values of Δt significantly greater than 3.0 EFPY(100 MW) would result in $P(F/E)_{\max}$ not satisfying the maximum permissible value (1×10^{-5}). Thus, based on this consideration, Δt must be limited to 3.0 EFPY(100 MW).

As indicated in Appendix I, including the beneficial effect of hydro testing in the calculation of $P(F/E)$ for the E/F condition reduces $P(F/E)(E/F)$ to zero, which, of course is the intent of the hydro test.

If hydro-test conditions equal to the E/F conditions were included in the analysis of the beneficial effects of hydro testing, the maximum value of $P(F/E)(E/F)$, assuming a vessel lifetime of 50 EFPY(100 MW), would be reduced from 1.7×10^{-5} to 2.6×10^{-6} , which is quite conservative and nearly satisfies the proposed limit of 1×10^{-6} . The corresponding mean frequency of failure is $< 2 \times 10^{-8} \text{y}^{-1}$, which satisfies the limit of $5 \times 10^{-6} \text{y}^{-1}$.

5. Summary and conclusions

The purpose of the HFIR vessel periodic hydrostatic proof test is to demonstrate with a very high degree of confidence that the vessel is safe to operate under "worst-case" conditions during the time between tests (Δt). Calculation of the hydro-test conditions (pressure, temperature, and frequency) accounts for vessel degradation (radiation-induced reduction in fracture toughness and flaw growth due to fatigue and corrosion) during Δt in a conservative manner. Failure of the vessel during a hydro test is not a safety issue in the sense that fuel does not reside in the vessel during the test. Even so, probabilistic calculations are performed for the hydro-test conditions and the results compared to reasonable acceptance criteria.

As a defense-in-depth measure, the probability of vessel failure is also calculated for the "worst-case" operating condition. This probability must be less than a value based on NRC safety studies for commercial PWR power plants. Because the consequences of failure of the HFIR vessel are so much less than those for the relatively large commercial PWRs, the criteria selected are very conservative.

Increasing the size of the HB-2 beam tube increases the dose rate at the nozzle corner by a factor of 10 and a lesser amount at more distant points. The effect, for specific analytical evaluation procedures and models and corresponding sets of criteria and input data, is to increase the required frequency of hydro testing and to increase the calculated probability of failure.

The calculated probability has been limited to acceptable values, and reasonable times between hydro tests have been maintained by introducing new data and more

realistic criteria, including recently calculated stresses and neutron and gamma fluxes, a reduction in the specified flaw density for base material, using the latest radiation damage correlation, including the beneficial effects of hydro testing in the calculation of the probability of failure, and increasing the maximum permissible probability of failure for the hydro-test condition. The new limits on probability of failure for the hydro test are 1×10^{-5} for the best-estimate conditional probability of failure and $1 \times 10^{-3} \text{y}^{-1}$ for the mean frequency of failure. The calculated maximum values, based on a vessel life of 50 EFPY(100 MW), are 9.5×10^{-6} and $1.4 \times 10^{-4} \text{y}^{-1}$, and the specified times between hydro tests are 1.0, 2.0, and 3.0 EFPY(100 MW) for the first, second, and subsequent time periods, respectively .

For the worst-case operating condition, the maximum permissible value of the conditional probability of failure is 1×10^{-6} and of the mean frequency of failure is $5 \times 10^{-6} \text{y}^{-1}$. The calculated values at 50 EFPY(100 MW), not accounting for the beneficial effect of hydro testing, are 1.7×10^{-5} and $1.5 \times 10^{-8} \text{y}^{-1}$, the latter of which satisfies the criteria. Including the effect of hydro testing in the calculation reduces the conditional probability to less than 1×10^{-6} .

Based on the results of the studies discussed in this report, it is concluded that with the enlarged HB-2 and HB-4 beam tubes installed the HFIR vessel can be operated for a total of 50 EFPY(100 MW).

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Fig. 1. Present and new designs of HB-2 beam tube at location of nozzle.

FUTURE HB-4 BEAM TUBE
AT VESSEL NOZZLE

CURRENT HB-4 BEAM TUBE
AT VESSEL NOZZLE

Fig. 2. Present and new designs of HB-4 beam tube at location of nozzle.

Fig. 3. Δ NDTT vs total (n+y) dpa for HFIR-type pressure vessel materials irradiated at low temperature (120—200°F); from Ref. 23.

Fig. 4. $\Delta NDTT$ vs $dpa(n+y)$.

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Fig. 5. $P(F/E)(HT)$ and $P(F/E)(E/F)$ vs t without hydro-test effect.

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Fig. 6. $P(F/E)(HT)$ vs time with and without the beneficial effect of hydro testing considered.

APPENDIX A

CALCULATION OF MAXIMUM PERMISSIBLE TIME BETWEEN HYDRO TESTS

A.1 Criteria and General Methods of Analysis

The maximum permissible time between hydro tests (Δt) is calculated by (1) specifying that the potential for vessel failure during the hydro test must be greater than that for the credible worst-case loading condition [referred to as the emergency/faulted (E/F) condition], and (2) specifying that the probability of failure for the hydro test satisfies a maximum permissible value unrelated to the E/F condition. Both criteria must be satisfied.

Because of radiation-induced embrittlement, the greatest potential for vessel failure is related to unstable propagation of sharp, crack-like defects (flaws) that might exist in the vessel. This potential can be evaluated both deterministically and probabilistically. The deterministic approach is relatively conservative and was used for determining Δt for the present design.^{1,5,7,8} When applied to the upgrade design (enlarged HB-2 and HB-4 beam tubes), the values of Δt are less than desired, and, thus, the probabilistic approach has also been applied. Details of both analyses are included below.

A.2 Deterministic Equal-Potential Approach

A.2.1 Method of analysis

With all uncertainties accounted for, the above criterion can be expressed as

$$\frac{K_I(HT)}{K_{Ic}(HT)} = \frac{K_I(E/F)}{K_{Ic}(E/F)} \quad (A.1)$$

where,

- $K_I(HT)$ = flaw-tip stress intensity factor for hydro-test loading conditions
- $K_{Ic}(HT)$ = material fracture toughness at tip of flaw for hydro-test conditions
- $K_I(E/F)$ = flaw tip stress intensity factor for emergency/faulted condition
- $K_{Ic}(E/F)$ = material fracture toughness at tip of flaw for E/F conditions

In general,

$$K_I(HT) = \left[p(HT) \sum_i \left(\frac{\sigma}{p} \right)_i c_i + \sigma_c c_c(a) + \sigma_r c_r \right] \sqrt{\pi a} \quad (A.2)$$

$$K_I(E/F) = \left[p(E/F) \sum_i \left(\frac{\sigma}{p} \right)_i c_i + \sigma_c c_c(a) + \sigma_r c_r \right] \sqrt{\pi(a + \Delta a)} \quad (A.3)$$

$$K_{Ic}(HT) = A + B \exp\{C[T_v(HT) - RT_{NDT}(t = t(HT))]\} \quad (A.4)$$

$$K_{Ic}(E/F) = A + B \exp\{C[T_v(E/F) - RT_{NDT}(t = t(HT) + \Delta t)]\} \quad (A.5)$$

$$\begin{aligned} a &= \text{flaw depth for } t = t(HT) \\ \Delta a &= \dot{a} \cdot \Delta t \end{aligned} \quad (A.6)$$

$$\begin{aligned} \dot{a} &= \text{rate of flaw growth} \\ RT_{NDT} &= \text{reference nil-ductility transition temperature} \\ &= RT_{NDT_0} + e \cdot \Delta RT_{NDT} \end{aligned} \quad (A.7)$$

$$\begin{aligned} RT_{NDT_0} &= \text{initial (zero dose) value of } RT_{NDT} \\ \Delta RT_{NDT} &= \text{increase in } RT_{NDT} \text{ due to radiation} \\ &= \Delta NDTT \\ e &= \text{uncertainty factor applied to } \Delta RT_{NDT} \\ \Delta NDTT &= \text{increase in nil-ductility transition temperature} \\ &= f(dpa) \end{aligned} \quad (A.8)$$

$$\begin{aligned} dpa(t \geq 22.6 \text{ EFY}(100\text{MW})) &= dpa(t < 22.6 \text{ EFY}(100\text{MW})) \times 22.6 \\ &+ dpa(t \geq 22.6) \times (t - 22.6) \end{aligned} \quad (A.9)$$

$$\begin{aligned} dpa &= f(a) \\ (\sigma/p)_i &= \text{pressure-induced stress of the } i\text{th type (bending or membrane)} \\ \sigma_c &= \text{differential-expansion stress in cladding} \\ c_c &= \text{geometry factor for cladding differential-expansion stress} \\ \sigma_r &= \text{residual stress} \\ c_r &= \text{geometry factor for residual stress} \\ c_i &= \text{geometry factor for bending or membrane stress} \\ t &= \text{time of operation} \\ t(HT) &= \text{time of hydro test} \\ \Delta t &= \text{time between hydro tests} \\ A, B, C &= \text{constants} \\ T_v(HT) &= \text{vessel temperature during hydro test} \\ T_v(E/F) &= \text{vessel temperature during } E/F \text{ operating condition} \\ p(HT) &= \text{pressure during hydro test} \\ p(E/F) &= \text{pressure during } E/F \text{ condition} \end{aligned}$$

As discussed in Sect. 4.3.3, values of $\Delta NDTT$ are obtained from the Remec curve in Figs. 3 and 4. The equation describing this curve is

$$\Delta NDTT = 807.7 + 182.6 (\ln dpa) + 10.63 (\ln dpa)^2 \quad (A.10)$$

The ratio $K_{Ic}(HT)/K_{Ic}(E/F)$ is only slightly dependent on flaw depth (a), and $\dot{a}\Delta t \ll a$ (see Appendix H). Thus, if the cladding term were removed from Eqs. (A.2) and (A.3), Δt would be insensitive to the value of $\underline{a}$, as demonstrated in Refs. 5 and 7, and this simplifies the analysis. The cladding effect is small for flaw sizes of interest and can be accommodated by the large degree of conservatism in the general method of analysis. Thus, to simplify the analysis, the cladding term was removed, the term $\dot{a}\Delta t/a$ was set equal to zero, and the above ratio of K_{Ic} values was evaluated at the inner surface.

Because the function $f(dpa)$ in Eq. (A.8) is not linear with time, it is more convenient to solve Eqs. A.1–A.10 for $p(HT)$ rather than Δt . This yields

$$p(HT) = \frac{K_{Ic}(HT)}{K_{Ic}(E/F)} \cdot p(E/F) + S \left[\frac{K_{Ic}(HT)}{K_{Ic}(E/F)} - 1 \right], \quad (A.11)$$

where

$$S = \frac{\sigma_r c_r}{\sum_i \left(\frac{\sigma}{p} \right)_i c_i} \quad (A.12)$$

A.2.2 Trends and Analytical Procedure

As discussed in Ref. 1, plots of Δt and $p(HT)$ vs e result in a minimum value of Δt and a maximum value of $p(HT)$, and, of course, precise values of e are not known. Thus, Eq. A.11 is solved by trial and error to find the value of e that results in the maximum value of $p(HT)$ for a given value of Δt and to find the value of Δt that results in the desired maximum value of $p(HT)$.

The values of some of the input parameters ($\sigma_b, c_b, \sigma_r, c_r, dpa, RT_{NDT_0}$) are location dependent, and thus Eq. A.11 is solved for "all" locations on the vessel to determine the minimum value of Δt . This is the value of Δt that must be specified.

The HB-2 nozzle corner results in the smallest values of Δt , but because of its relatively high initial fracture toughness, the HB-2 nozzle corner satisfies the ASME Code for a short time after implementation of the beam-tube design changes. For this time period, Δt is based on the HB-2 nozzle weld, which has the second highest potential for the minimum value of Δt .

The value of Δt arrived at in the above manner increases with increasing values of $t(HT)$. This is a result of K_{Ic} approaching an asymptote with increasing time. (When $dK_{Ic}/dt = 0$, $\Delta t = \infty$, if $\dot{a}/a \ll 1$.) If a single value of Δt were to be specified, it would

have to be the value corresponding to the minimum value of $t(HT)$. Presumably, it is more appropriate to take advantage of the increasing permissible value.

A.2.3 Input Data

Input data for calculating Δt are included in Table A.1, and a brief discussion of their sources follows.

Increasing $p(HT)$ increases Δt but also increases the probability of vessel failure during the hydro test. Thus, if a single value of $p(HT)$ is to be specified, it should result in the maximum permissible value of the probability of failure at the time of the last hydro test. As a first trial, the presently specified value of $p(HT)$ (900 psid) was used in this study for calculating Δt and the probability of failure.

The value of $p(E/F)$ is the vessel differential pressure at the core horizontal midplane corresponding to the vendor-specified "maximum" burst pressure of the primary-system rupture disk.

The value of $T_v(HT)$ is the specified value, and $T_v(E/F)$ and RT_{NDT_0} are the minimum expected values. This combination results in the minimum value of Δt .

For the nozzle corner, residual stresses are essentially zero, and thus $S=0$, but residual stresses must be considered at the weld.

The constants A , B , and C correspond to the ASME lower-bound K_{Ic} curve.¹⁹ The shape of the K_{Ic} curve (K_{Ic} vs $T-RT_{NDT}$) influences the value of Δt , and thus several possible variations have been considered.¹ It was concluded that the Ref. 19 curve is appropriate for these studies.

The time for the changeover from the original HB-2 and HB-4 beam tube designs to the larger-diameter designs was specified as 22.6 EFPY(100 MW), and it was assumed that a hydro test would be performed at that time.⁹ Values of dpa for the inner surface of the HB-2 nozzle corner and weld for $t < 22.6$ EFPY(100 MW) and $t \geq 22.6$ EFPY(100 MW) were taken from tabulated data related to figures containing iso dpa plots in Appendix E.

Table A.1. Input for calculation of Δt

Parameter	Value
A , ksi $\sqrt{\text{in.}}$	33.2
B , ksi $\sqrt{\text{in.}}$	20.73
C , $^{\circ}\text{F}^{-1}$	0.02
$p(HT)$, psid	900
$p(E/F)$, psid	679
$T_v(HT)$, $^{\circ}\text{F}$	85
$T_v(E/F)$, $^{\circ}\text{F}$	80
$RT_{NDT_0}(-2\sigma)$, $^{\circ}\text{F}$	-144 (HB-2 nozzle) -24 (HB-2 nozzle weld)
ΔRT_{NDT} , $^{\circ}\text{F}$	From Fig. 3 and/or Eq. A.10
dpa , $dpa/\text{EFPY}(100 \text{ MW})$	
$t \leq 22.6 \text{ EFPY}(100 \text{ MW})$	1.21×10^{-4} (HB-2 nozzle corner) 6.28×10^{-5} (HB-2 nozzle weld)
$t > 22.6 \text{ EFPY}(100 \text{ MW})$	1.25×10^{-3} (HB-2 nozzle corner) 9.15×10^{-5} (HB-2 nozzle weld)
S , psi	0 (HB-2 nozzle corner) 326 (HB-2 nozzle weld)

A.2.4 Calculations

A.2.4.1 HB-2 nozzle corner

The calculation begins with $t(HT) = 22.6 \text{ EFPY}(100 \text{ MW})$. Assuming a value of Δt , $p(HT)$ is calculated for several values of e to obtain $p(HT)_{\text{max}}$. The process is repeated until $p(HT)_{\text{max}} = 900 \pm 1 \text{ psi}$. Calculations are included below for $\Delta t < 4 \text{ EFPY}(100 \text{ MW})$, showing the iterations on e and the converged values of Δt . The results are summarized in Table A.2.

1. $t(HT) = 22.60$, $\Delta t = 0.40$ (trial)

$$\begin{aligned} \text{From Eq. A.9: } dpa(t = 22.6) &= 1.21 \times 10^{-4} \times 22.6 = 2.735 \times 10^{-3} \\ dpa(t = 22.6 + 0.40 = 23.00) &= 1.21 \times 10^{-4} \times 22.6 \\ &+ 1.25 \times 10^{-3} \times 0.40 = 3.235 \times 10^{-3} \end{aligned}$$

$$\begin{aligned} \text{From Eq. A10: } \Delta NDTT(t = 22.60) &= 100.3 \\ \Delta NDTT(t = 23.00) &= 110.2 \end{aligned}$$

$$\begin{array}{rcccl} \text{From Eq. A11: } e(\text{trials}) & 1.35 & 1.4 & 1.45 & 1.5 \\ p(HT) & 900 & 901 & 901 & 901 \end{array}$$

$$\therefore \Delta t = 0.40$$

2. $t(HT) = 22.6 + 0.40 = 23.00, \Delta t = 0.49$ (trial)

$$\Delta NDTT(t = 23.00) = 110.2$$

$$dpa(t = 23.00 + 0.49 = 23.49) = 3.847 \times 10^{-3}, \Delta NDTT(t = 23.49) = 121.0$$

e	1.2	1.25	1.3	1.4
p(HT)	900	901	901	898

$$\therefore \Delta t = 0.49$$

3. $t(HT) = 23.00 + 0.49 = 23.49, \Delta t = 0.60$ (trial)

$$\Delta NDTT(t = 23.49) = 121.0$$

$$dpa(t = 23.49 + 0.60 = 24.09) = 4.597 \times 10^{-3}, \Delta NDTT(t = 24.09) = 132.8$$

e	1.05	1.1	1.15	1.2	1.25
p(HT)	898	899	900	900	898

$$\therefore \Delta t = 0.60$$

4. $t(HT) = 23.49 + 0.60 = 24.09, \Delta t = 0.75$ (trial)

$$\Delta NDTT(t = 24.09) = 132.8$$

$$dpa(t = 24.09 + 0.75 = 24.84) = 5.535 \times 10^{-3}, \Delta NDTT(t = 24.84) = 145.9$$

e	1.0	1.05	1.1
p(HT)	900	901	900

$$\therefore \Delta t = 0.75$$

5. $t(HT) = 24.09 + 0.75 = 24.84, \Delta t = 0.95$ (trial)

$$\Delta NDTT(t = 24.84) = 145.9$$

$$dpa(t = 24.84 + 0.95 = 25.79) = 6.722 \times 10^{-3}, \Delta NDTT(t = 25.79) = 160.3$$

e	0.85	0.9	0.95	1.0	1.05
p(HT)	898	900	901	901	899

$$\therefore \Delta t = 0.95$$

6. $t(HT) = 24.84 + 0.95 = 25.79, \Delta t = 1.20$ (trial)

$$\Delta NDTT(t = 25.79) = 160.3$$

$$dpa(t = 25.79 + 1.20 = 26.99) = 8.222 \times 10^{-3}, \Delta NDTT(t = 26.99) = 176.1$$

e	0.8	0.85	0.9	0.95
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p(HT)	899	901	901	899
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$$\therefore \Delta t = 1.20$$

7. $t(HT) = 25.79 + 1.20 = 26.99, \Delta t = 1.53$ (trial)

$$\Delta NDTT(t = 26.99) = 176.1$$

$$dpa(t = 26.99 + 1.53 = 28.52) = 1.0135 \times 10^{-2}, \Delta NDTT(t = 28.52) = 193.4$$

e	0.75	0.8	0.85
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p(HT)	899	900	899
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$$\therefore \Delta t = 1.53$$

8. $t(HT) = 26.99 + 1.53 = 28.52, \Delta t = 1.98$ (trial)

$$\Delta NDTT(t = 28.52) = 193.4$$

$$dpa(t = 28.52 + 1.98 = 30.50) = 1.2610 \times 10^{-2}, \Delta NDTT(t = 30.50) = 212.4$$

e	0.65	0.7	0.75	0.8
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p(HT)	898	901	901	898
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$$\therefore \Delta t = 1.98$$

9. $t(HT) = 28.52 + 1.98 = 30.50, \Delta t = 2.55$ (trial)

$$\Delta NDTT(t = 30.50) = 212.4$$

$$dpa(t = 30.50 + 2.55 = 33.05) = 1.5797 \times 10^{-2}, \Delta NDTT(t = 33.05) = 233.2$$

e	0.60	0.65	0.70
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p(HT)	898	900	899
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$$\therefore \Delta t = 2.55$$

10. $t(HT) = 30.50 + 2.55 = 33.05$, $\Delta t = 3.35$ (trial)

$$\Delta NDTT(t = 33.05) = 233.2$$

$$dpa(t = 33.05 + 3.35 = 36.40) = 1.9985 \times 10^{-2}, \Delta NDTT(t = 36.40) = 256.0$$

e	0.55	0.6	0.65
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$p(HT)$	898	900	898
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$\therefore \Delta t = 3.35$

A.2.4.2 HB-2 nozzle weld

1. $t(HT) = 22.60$, $\Delta t = 3.0$ (trial)

$$dpa(t = 22.60) = 6.28 \times 10^{-5} \times 22.6 = 1.419 \times 10^{-3}$$

$$dpa(t = 25.60) = 1.419 \times 10^{-3} + 3.00 \times 9.15 \times 10^{-5} = 1.694 \times 10^{-3}$$

$$\Delta NDTT(t = 22.60) = 67.39$$

$$\Delta NDTT(t = 22.60 + 3.00 = 25.60) = 75.36$$

e	0.8	0.9	1.0	1.1
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$p(HT)$	833	835	835	834
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Because the "required" value of $p(HT)$ is less than 900 psi, the maximum permissible value of Δt is greater than 3.0 EFPY(100 MW). In Sect. 3 and Appendix B, it is mentioned and demonstrated that the nozzle corner satisfies the ASME Code until $t \approx 22.6 + 1.5 = 24.1$ EFPY(100 MW). Thus, from $t = 22.6$ to $t = 24.1$ EFPY, the nozzle weld dictates Δt , and a value of 1.49 EFPY(100 MW) is acceptable as the initial value (Table A.2). At $22.60 + 1.49 = 24.09$ EFPY and thereafter, the nozzle corner dictates Δt , and the initial value is 0.75 EFPY(100 MW). Thus, $\Delta t_1 = 1.49$, $\Delta t_2 = 0.75$, $\Delta t_3 = 0.95$,

**Table A.2. Summary of calculated values of $t(HT)$ and Δt
for HB-2 nozzle corner**

$t(HT)$	$t(HT) - 22.6$	Δt
EFPY(100 MW)		
22.60	0	0.40
23.00	0.40	0.49
23.49	0.89	0.60
24.09	1.49	0.75
24.84	2.24	0.95
25.79	3.19	1.20
26.99	4.39	1.53
28.52	5.92	1.98
30.50	7.90	2.55
33.05	10.45	3.35

A.3 Probabilistic Equal-Potential Approach

For the probabilistic approach to calculating Δt , the condition that must be satisfied is

$$P(F|E)(HT) \geq P(F|E)(EIF) \quad (A.13)$$

The two plots of $P(F/E)$ vs t are included in Fig. 5 for the case of no assumed beneficial effect of hydro testing. The minimum value of the maximum permissible time between hydro tests is the minimum difference in times between the two curves for the same value of $P(F/E)$. The value of Δt so derived is 6.5 EFPY(100 MW). This value was multiplied by an uncertainty factor of 0.5 to accommodate second-order uncertainties not included in the $P(F/E)$ analysis. Thus, $\Delta t = 3$ EFPY(100 MW). Details of the analysis are included in Appendix I.

A.4 Δt Based on $P(F/E)(HT) \leq$ Maximum Permissible Value

The probabilistic evaluation in Appendix I and the corresponding results in Fig. 6 indicate that the beneficial effects of hydro testing must be included in the calculation of $P(F/E)(HT)$ and that Δt must be no greater than 3 EFPY(100 MW) for $22.6 t(HT) \leq t(HT) \leq 40$ EFPY(100 MW) if the proposed maximum permissible value of $P(F/E)(HT)$ is to be satisfied.

A.5 Comments and Recommendations

A comparison of Δt values obtained from the deterministic and probabilistic equal-potential approaches indicates the magnitude of the conservatism included in the deterministic approach, which is not excessive for present operating conditions but is for the upgrade design.

Based on the equal-probability analysis, $\Delta t = 3.0$ EFPY(100 MW) is acceptable beginning at $t = 22.6$ EFPY(100 MW), that is, the time of restart with the upgrade design. Also, achieving the desired beneficial effect of hydro testing in reducing $P(F/E)(HT)$ to acceptable values requires a limit of 3.0 EFPY(100 MW) up to $t \approx 40$ EFPY(100 MW), with larger values being permissible thereafter. Because of the big increase in dose rate at HB-2, associated with the upgrade, it is recommended that Δt for the first and second hydro-test periods be limited to 1.0 and 2.0 EFPY(100 MW), respectively, and that thereafter Δt be limited to 3.0 EFPY(100 MW).

APPENDIX B

ASME CODE FRACTURE-MECHANICS ANALYSIS FOR HB-2 NOZZLE CORNER

The ASME Code requires that¹⁹

$K_{IT} \leq K_{IR}$ for normal/upset (N/U) conditions, and
 $K_{IT} \leq K_{IC}$ for emergency/faulted (E/F) conditions,

where

$$K_{IT}(N/U) = 2K_{Ip} + (K_{Ith} + K_{Ir}) \quad (B.1)$$

$$K_{IT}(E/F) = \sqrt{2} K_{Ip} + (K_{Ith} + K_{Ir}) \quad (B.2)$$

From Ref. 19,

$$K_{IC} = 33.2 + 20.73 \exp \left[0.02(T_v - RT_{NDT_o} - \Delta RT_{NDT}) \right] \quad (B.3)$$

$$K_{IR} = 26.8 + 12.44 \exp \left[0.0145(T_v - RT_{NDT_o} - \Delta RT_{NDT}) \right], \quad (B.4)$$

where

K_I	=	stress intensity factor ($ksi \sqrt{in.}$)
K_{IC}	=	ASME lower-bound static fracture toughness ($ksi \sqrt{in.}$)
K_{IR}	=	ASME lower-bound dynamic/arrest fracture toughness ($ksi \sqrt{in.}$)
RT_{NDT_o}	=	initial (zero dose) value of reference nil-ductility transition temperature ($^{\circ}F$)
ΔRT_{NDT}	=	increase in RT_{NDT} due to irradiation
$\Delta NDTT$	=	increase in nil-ductility transition temperature [from Eq. (E.1)], $^{\circ}F$
T_v	=	vessel temperature ($^{\circ}F$)

Subscripts:

p	=	primary loads
r	=	residual stress
th	=	thermal loads
T	=	total

For the HB-2 nozzle corner,⁵

$$K_{Ip} = F(a/r_n) \sigma_h \sqrt{\pi a} \quad (B.5)$$

$$K_{Ith} = 0, K_{Ir} = 0$$

$$F(a/r_n) = 2.5 - 5.13 (a/r_n) + 5.63 (a/r_n)^2, a/r_n \leq 0.40 \quad (B.6)$$

$$a/r_n = 0.137a \text{ in}^{-1} \quad (B.7)$$

$$\sigma_h = 16p, \quad (B.8)$$

where

- a = crack depth (Fig. B.1)
- p = primary-system pressure
- r_n = radius defining location of nozzle-corner flaw (Fig. B.1)

For N/U conditions:

$$\begin{aligned} \sigma_h &= 0.508 \times 16 = 8.1 \text{ ksi} \\ a &= 1 \text{ in. (specified by ASME Code}^{37}) \\ a/r_n &= 1 \times 0.137 = 0.137 \end{aligned}$$

$$F(a/r_n) = 2.5 - 5.13 (0.137) + 5.63 (0.137)^2 = 1.90$$

$$K_{IT(N/U)} = 2 \times 1.90 \times 8.1 \sqrt{\pi \times 1} = 55 \text{ ksi } \sqrt{\text{in.}}$$

$$\begin{aligned} T_v &= 80^\circ\text{F} \\ RT_{NDT_0} &= -110^\circ\text{F} \end{aligned}$$

$$\therefore K_{IR} = 26.8 + 12.44 \exp[0.0145 (190 - \Delta RT_{NDT})] \quad (B.9)$$

$\Delta NDTT$: See Appendix E, Eq. (E.1).

From Appendix A and using Eqs. (E.1) and (B.9),

t EFPY(100 MW)	dpa	$\Delta NDTT$ °F	K_{IR} ksi $\sqrt{in.}$
22.60	2.735×10^{-3}	100.3	72
23.00	3.235×10^{-3}	110.2	66
23.49	3.847×10^{-3}	121.0	61
24.09	4.597×10^{-3}	132.8	55
24.84	5.535×10^{-3}	145.9	50

For E/F condition:

$$\sigma_h = 0.679 \times 16 = 10.9 \text{ ksi}$$

$$K_{IT}(E/F) = \sqrt{2} \times 1.90 \times 10.9 \sqrt{\pi \cdot 1} = 52 \text{ ksi } \sqrt{in.}$$

$$K_{IC} > K_{IR} \therefore E/F \text{ not limiting.}$$

Conclusion: Code satisfied for HB-2 nozzle corner until $t = 24.09$ EFPY(100 MW).

Fig. B.1. Nozzle-corner flaw.

APPENDIX C

VESSEL REGIONS SPECIFIED FOR PROBABILISTIC FRACTURE MECHANICS ANALYSIS AND THEIR SURFACE AREAS

C.1 Regions Considered

With reference to Figs. C.1, C.2, and C.3, the regions of the vessel defined for the probabilistic analysis are the beam-tube nozzle "corners;" the annular space between the nozzle-corner region and the nozzle weld; the nozzle weld; the vessel-shell circumferential weld below the core; a portion of the vessel-shell axial weld; and a limited height of the vessel shell, exclusive of the welds and beam-tube nozzles. The nozzle regions are subdivided (Fig. C.3) to accommodate azimuthal variations in the stresses; the most severe condition (stress and dpa) are assumed to exist in the same quadrant; and this quadrant is assumed to be typical of the others. Each of the regions is represented by a surface area that is multiplied by a flaw surface density to obtain the number of flaws per region, all flaws being treated as surface flaws, which have the greatest potential for propagation. The flaw surface density is assumed to be equal to the flaw volumetric density times the wall thickness, and thus the projected area of the nozzle corner is used, and the bore of the nozzle is neglected. Area calculations are presented below.

C.2 Calculation of Region Areas

C.2.1 HB-2

HB-2 is essentially axisymmetric, and thus the assumption of one quadrant being typical is realistic.

C.2.1.1 Nozzle corner (regions 1, 2, and 3)

The nozzle-corner area is defined as

$$A_{nc} = \pi(r_A^2 - r_i^2)f,$$

where r_A and r_i are defined in Figs. C.2 and C.3, and f is the fraction of the quadrant corresponding to the subregion.

For HB-2, $r_i = 7$ in., and, based on the azimuthal stress distribution, the following were specified:

$$\begin{aligned} r_A &= 9.3 \text{ in.} \\ f_1 &= f_2 = 0.375 \\ f_3 &= 0.250 \end{aligned}$$

Thus,

$$\begin{aligned} A_1 &= \pi(9.3^2 - 7^2) \times 0.375 = 44 \text{ in.}^2 = 0.31 \text{ ft}^2 \\ A_2 &= A_1 = 0.31 \text{ ft}^2 \\ A_3 &= \frac{2}{3} A_1 = \frac{2}{3} \times 0.31 = 0.21 \text{ ft}^2 \end{aligned}$$

C.2.1.2 Annular region between nozzle corner and nozzle weld (regions 4 and 5)

The annular area is defined as

$$A_A = \pi(r_w^2 - r_A^2)f.$$

For HB-2,

$$\begin{aligned} r_w &= 15 \text{ in.} \\ r_A &= 9.3 \text{ in.} \\ f &= 0.5 \text{ (specified on basis of stress distribution)} \end{aligned}$$

Thus,

$$\begin{aligned} A_4 &= \pi(15^2 - 9.3^2) 0.5 = 218 \text{ in.}^2 = 1.5 \text{ ft}^2 \\ A_5 &= A_4 = 1.5 \text{ ft}^2 \end{aligned}$$

C.2.1.3 Nozzle weld (regions 6 and 7)

The nozzle weld region is defined as

$$A_{NW} = 2\pi r_w \overline{W} f$$

For HB-2,

$$\begin{aligned} r_w &= 15 \text{ in.} \\ \overline{W} &= 1.375/2 = 0.688 \text{ in.} \\ f_6 &= 0.7 \text{ (specified on basis of stress distributions)} \\ f_7 &= 0.3 \end{aligned}$$

Thus,

$$\begin{aligned} A_6 &= 2\pi \times 15 \times 0.688 \times 0.7 = 45 \text{ in.}^2 = 0.32 \text{ ft}^2 \\ A_7 &= \frac{3}{7} A_6 = 0.14 \text{ ft}^2 \end{aligned}$$

C.2.2 HB-3

HB-3 is not axisymmetric. However, the stresses are nearly axisymmetric and the contribution of HB-3, relative to HB-2, to the calculated probability of failure is small enough that the quadrant with the highest values of dpa can be used to conservatively estimate the contribution. These areas are calculated as for HB-2.

For HB-3,

$$\begin{aligned} r_A &= 5.3 \text{ in.} \\ r_i &= 4.0 \text{ in.} \end{aligned}$$

$$\begin{aligned}
 r_w &= 10.0 \text{ in.} \\
 \bar{W} &= 0.688 \text{ in.} \\
 f_8 &= f_9 = 0.375 \\
 f_{10} &= 0.250 \\
 f_{11} &= f_{12} = 0.5 \\
 f_{13} &= 0.7 \\
 f_{14} &= 0.3
 \end{aligned}$$

C.2.2.1 Nozzle corner (regions 8, 9, and 10)

$$A_8 = \pi(5.3^2 - 4.0^2) 0.375 = 14.2 \text{ in.}^2 = 0.10 \text{ ft}^2$$

$$A_9 = A_8 = 0.10 \text{ ft}^2$$

$$A_{10} = \frac{2}{3} A_8 = \frac{2}{3} \times 0.10 = 0.07 \text{ ft}^2$$

C.2.2.2 Annular region between nozzle corner and nozzle weld (regions 11 and 12)

$$A_{11} = \pi(10^2 - 5.3^2) 0.5 = 113 \text{ in.}^2 = 0.78 \text{ ft}^2$$

$$A_{12} = A_{11} = 0.78 \text{ ft}^2$$

C.2.2.3 Nozzle weld (regions 13 and 14)

$$A_{13} = 2\pi \times 10 \times \frac{1.375}{2} \times 0.7 = 30.2 \text{ in.}^2 = 0.21 \text{ ft}^2$$

$$A_{14} = \frac{3}{7} A_{13} = 0.09 \text{ ft}^2$$

C.2.3 HB-1 and HB-4 (regions 15–28)

Areas are essentially the same as for HB-3.

$$A_{15} = A_{22} = A_8 = 0.10 \text{ ft}^2$$

$$A_{16} = A_{23} = A_9 = 0.10 \text{ ft}^2$$

$$A_{17} = A_{24} = A_{10} = 0.07 \text{ ft}^2$$

$$A_{18} = A_{25} = A_{11} = 0.78 \text{ ft}^2$$

$$A_{19} = A_{26} = A_{12} = 0.78 \text{ ft}^2$$

$$A_{20} = A_{27} = A_{13} = 0.21 \text{ ft}^2$$

$$A_{21} = A_{28} = A_{14} = 0.09 \text{ ft}^2$$

C.2.4 Vessel circumferential weld below core (region 29, see Fig. C.1)

$$A_{29} = 2\pi r_v \bar{W} = 2\pi \times 48 \times \frac{1.375}{2}$$

$$= 207 \text{ in.}^2 = 1.4 \text{ ft}^2$$

C.2.5 Vessel axial weld (region 30, see Fig. C.1)

$$A_{30} = l \bar{W} = 50 \text{ in.} \times \frac{1.375}{2} = 34 \text{ in.}^2 = 0.24 \text{ ft}^2$$

C.2.6 Vessel shell (region 31, see Fig. C.1)

$$A_{31} = 2\pi r_v l = 2\pi \times 48 \times 50 = 1.51 \times 10^4 \text{ in.} = 100 \text{ ft}^2$$

Fig. C.1. Vertical cross section of HFIR pressure vessel, including core [fuel (F) and beryllium reflector (R)].

Fig. C.2. Horizontal cross section of HFIR pressure vessel and core at horizontal midplane of core.

Fig. C.3. Views of nozzle indicating defined regions for P(F/E) analysis.

APPENDIX D

FLAW-SIZE-DISTRIBUTION FUNCTION

The flaw-size-distribution function and flaw size truncation used in the present analysis are the same as those used in previous analyses^{2,5} and endorsed by the NRC for evaluation of PWRs.¹⁴ The number of flaw sizes, however, is greater than before (15 compared to 8) and thus the numerical representation of the flaw-size-distribution function is different. The distribution function is represented by

$$f(a)B(a)$$

where $f(a)$ is the distribution function prior to the final fabrication-stage inspection and repair, and $B(a)$ is the probability of "non detection" during the final fabrication-stage inspection and includes detected flaws that were not repaired.

The relative number of flaws of a specified average size following the final inspection and repair is

$$N(a_i) = \int_{\Delta a_i}^w f(a)B(a)da / \int_0^w f(a)B(a)da, \quad (D.1)$$

where w is the wall thickness, and Δa_i is the flaw-size increment about the average flaw size a_i . Values of $N(a_i)$ and $\sum_i N(a_i)$ are used in the calculation of $P(F/E)$.

The functions $f(a)$ and $B(a)$ used in previous studies are

$$f(a) = 0.16e^{-0.16a \text{ mm}^{-1}} \quad (D.2)$$

$$B(a) = 0.005 + 0.995e^{-0.113a \text{ mm}^{-1}} \quad (D.3)$$

Thus

$$\int_0^w f(a)B(a)da = 0.16 \int_0^{3 \times 25.4} (0.005e^{-0.16a} + 0.995e^{-0.273a})da$$

$$= 0.16 \left[\frac{0.005e^{-0.16a}}{-0.16} + \frac{0.995e^{-0.273a}}{-0.273} \right] \Bigg|_0^{3 \times 25.4}$$

$$= 0.588150$$

$$\int_{a_1}^{a_2} f(a)B(a)da$$

$$= 0.16 \left[\frac{0.005}{0.16} (e^{-0.16a_1} - e^{-0.16a_2}) + \frac{0.995}{0.273} (e^{-0.273a_1} - e^{-0.273a_2}) \right] \quad (D.4)$$

Values of Δa_i , a_i , $\int_{\Delta a_i} f(a)B(a)da / \int_0^w f(a)B(a)da$ are given in Table D.1.

It is of interest to note that the inspection and repair operation reduces the flaw density from N to $0.588 N$. As discussed in Sect. 4.3.2, however, the flaw densities specified for the $P(/E)$ analyses are those following final fabrication-stage inspection and repair and, thus, the specified flaw densities are not multiplied by 0.588.

Table D.1. Average flaw sizes and relative number of flaws for each

$\bar{a}_i$ (in.)	Δa_i (in.)	A_i^a	ΣA_i
0.125	0.1625 - 0	0.674297	0.67430
0.2	0.25 - 0.1625	0.147471	0.82177
0.3	0.35 - 0.25	0.088629	0.91040
0.4	0.45 - 0.35	0.044473	0.95487
0.5	0.55 - 0.45	0.022344	0.97722
0.6	0.65 - 0.55	0.011245	0.98846
0.7	0.75 - 0.65	0.005671	0.99413
0.8	0.85 - 0.75	0.002869	0.99700
0.9	0.95 - 0.85	0.001456	0.99846
1.0	1.05 - 0.95	0.000743	0.99920
1.1	1.15 - 1.05	0.000381	0.99958
1.2	1.25 - 1.15	0.000197	0.99978
1.3	1.35 - 1.25	0.000103	0.99988
1.4	1.45 - 1.35	0.000054	0.99993
1.5	3.00 - 1.45	0.000066	1.00000
	3.00 - 0	1.000000	

$$^a A_i = \int_{\Delta a_i} f(a) B(a) da / \int_0^{\infty} f(a) B(a) da$$

= relative number of flaws for each average flaw size based on a total of one flaw.

APPENDIX E

DAMAGE RATES AND *dpa*

E.1 $\Delta NDTT$ vs *dpa*

As discussed in the text (Sect. 4.3.3), Remec, et al.,²³ have determined that gammas contribute to radiation damage in the HFIR vessel, and that $\Delta NDTT$ correlates well with the sum of the neutron and gamma *dpa*. The correlation is shown in Figs. 3 and 4 and was used in these studies for estimating $\Delta NDTT$ for the vessel regions defined in Appendix C. The equation for the Remec curve is

$$\Delta NDTT = 807.7 + 182.6 \ln(dpa) + 10.63 [n(dpa)]^2, \text{ } ^\circ\text{F} \quad (\text{E.1})$$

E.2 Neutron and Gamma *dpa* Rates

Neutron and gamma fluxes and corresponding values of *dpa* values were calculated specifically for the HFIR vessel life-extension study and are reported in Ref. 24. Two-dimensional models (Figs. E.1 and E.2) were used in the *dpa* calculations for HB-2, which is nearly axisymmetric, and three-dimensional models were used for HB-3 and HB-4 (Figs. E.3, E.4, and E.5). Other details of the *dpa* calculations are included in Ref. 24, and typical results in the form of iso *dpa* plots are presented in Figs. E.6 through E.23. A two-dimensional (RZ) model (Fig. E.6) was used for calculating neutron and gamma boundary sources for the other models and for calculating flux and *dpa* values for regions far removed from the beam tubes. Values of *dpa* corresponding to the defined regions of the vessel for the probabilistic fracture-mechanics analysis are given below. As mentioned in Sect. 4.3.4, the *dpa* values used in the calculation of hydro-test conditions and in the $P(F/E)$ analysis were normalized using HFIR HB-2 and HB-4 neutron and gamma dosimetry data.²³ Reference 23 includes the corrected *dpa* values at the locations of the dosimeters, and these values were divided by the recently calculated *dpa* values²⁴ to obtain correction factors for other locations. The correction factors obtained for the HB-2 dosimeter (key 2, position 9) were used for regions 1 through 7. Factors obtained for the HB-4 dosimeters (key 4, positions 2 and 10) were used for regions 15 through 28. The averages of these factors were used for regions 8 through 14. Values of *dpa* for regions 29, 30, and 31 (shell areas away from nozzle effects) were obtained from a 2-D (RZ) calculation of the "core" in the absence of the beam tubes (Figs. E.6, E.7, and E.8). These values of *dpa* were normalized by comparing the 2-D(RZ) values with the normalized beam-tube-model values far enough from the beam tubes to be unaffected by the presence of the beam tubes (see Figs. E-9 through E.23). A summary of these data is included in Table E.1.

For operating times greater than ~ 25 EFY(100 MW), the calculated probability of failure is dominated by region 1 (HB-2 nozzle corner). This is because the proposed enlargement of the beam tube (maximum possible within the dimensional constraints) substantially reduces the neutron shielding (water) between the tube and nozzle bore. Anticipating that the increase in damage rate might be excessive, requiring a smaller tube diameter, the sensitivity of *dpa* at the HB-2 nozzle corner to beam-tube diameter was calculated. These results are presented in Fig. E.24.

The general equations used for calculating *dpa* (total) and *dpa* (total) are as follows:

$$dpa_{total} = dpa_{neutrons} + dpa_{prompt \text{ and } delayed \text{ gammas}}$$

$$dpa_i(t \geq 22.6 \text{ EFPY}(100\text{MW}), a, t)_i$$

$$= dpa_{total}(t < 22.6, a)_i \times 22.6 \text{ EFPY}(100\text{MW}) e^{-\lambda(t < 22.6)a}$$

$$+ dpa_{total}(t \geq 22.6, a)_i (t - 22.6) e^{-\lambda(t \geq 22.6)a}$$

where i indicates a specific region of the vessel, λ is an attenuation constant derived from data in Ref. 24, and a is flaw depth.

Table E.1. Normalization factors for calculated values of dpa rates

Applicable regions	Dosimeter location	dpa^{23} corrected	dpa^{24} calculated	Normalization factors
1-7	KEY 2: POSITION 9			
	η	2.22×10^{-12}	3.96×10^{-12}	0.560
	$\gamma(p + d)^a$	2.54×10^{-12}	2.33×10^{-12}	1.09
15-28	KEY 4:			
	POSITION 2			
	η	7.84×10^{-13}	1.17×10^{-12}	0.670
	$\gamma(p + d)$	1.25×10^{-12}	2.31×10^{-12}	0.541
	POSITION 10			
	η	1.01×10^{-12}	1.19×10^{-12}	0.849
	$\gamma(p + d)$	1.25×10^{-12}	2.10×10^{-12}	0.595
8-15	Average (2 & 10)			
	η			0.760
	$\gamma(p + d)$			0.568
29-31	Average (Keys 2 & 4)			
	η			0.660
	$\gamma(p + d)$			0.829
29-31	η			1.00
	$\gamma(p + d)$			1.00

^aprompt + delayed gammas

E.2.1 HB-2

E.2.1.1 Region 1 (nozzle corner)

$$\begin{aligned} d\dot{p}a(100 \text{ MW}, a = 0, t \leq 22.6) \\ = (1.92 \times 10^{-12} + 1.93 \times 10^{-12}) \frac{d\dot{p}a}{s} \times 3.15 \times 10^7 \frac{s}{y} \\ = 1.21 \times 10^{-4} / \text{EFPY}(100 \text{ MW}) \end{aligned} \quad (\text{E.2})$$

$$d\dot{p}a(t = 22.6, a = 0) = 1.21 \times 10^{-4} \times 22.6 = 2.74 \times 10^{-3} \quad (\text{E.3})$$

$$\begin{aligned} d\dot{p}a(100 \text{ MW}, a = 0, t > 22.6) \\ = (3.46 \times 10^{-11} + 5.1 \times 10^{-12}) \times 3.15 \times 10^7 \\ = 1.25 \times 10^{-3} / \text{EFPY}(100 \text{ MW}) \end{aligned} \quad (\text{E.4})$$

$$d\dot{p}a(t \geq 22.6, a, t) = 2.74 \times 10^{-3} e^{-\lambda(t \leq 22.6)} + 1.25 \times 10^{-3} (t - 22.6) e^{-\lambda(t > 22.6)}, \quad (\text{E.5})$$

where t is operating time in EFPY (100 MW). For region 1, $\lambda's \approx 0$.

E.2.1.2 Region 2 (nozzle corner)

Same as region 1.

E.2.1.3 Region 3 (nozzle corner)

Same as region 1.

E.2.1.4 Region 4 (nozzle annulus)

Radial location = 9.3 in. = 23.6 cm (see Fig. C.2).

$$\begin{aligned} d\dot{p}a(100 \text{ MW}, a = 0, t \leq 22.6) \\ = (7.08 \times 10^{-13} + 2.08 \times 10^{-12}) \times 3.15 \times 10^7 \\ = 8.78 \times 10^{-5} / \text{EFPY}(100 \text{ MW}) \end{aligned} \quad (\text{E.6})$$

$$d\dot{p}a (100 \text{ MW}, a = 0, t \geq 22.6)$$

$$= (9.48 \times 10^{-12} + 2.82 \times 10^{-12}) \times 3.15 \times 10^7 \quad (\text{E.7})$$

$$= 3.87 \times 10^{-4} / \text{EFPY}(100 \text{ MW})$$

$$\begin{aligned} d\dot{p}a(t \geq 22.6, a, t) &= 8.78 \times 10^{-5} \times 22.6 e^{-\lambda(t \leq 22.6)a} \\ &+ 3.87 \times 10^{-4} (t - 22.6) e^{-\lambda(t > 22.6)a} \end{aligned} \quad (\text{E.8})$$

$$\begin{aligned} \lambda(t \leq 22.6) &= \frac{1}{a_2 - a_1} \ln \frac{d\dot{p}a_1}{d\dot{p}a_2} \\ &= \frac{1}{137.0 - 118.7} \ln \frac{(7.08 \times 10^{-13} + 2.08 \times 10^{-12})}{(5.10 \times 10^{-13} + 4.13 \times 10^{-14})} \\ &= 0.089 \text{ cm}^{-1} = 0.22 \text{ in.}^{-1} \end{aligned}$$

$$\begin{aligned} \lambda(t > 22.6) &= \frac{1}{137.0 - 118.1} \ln \left(\frac{9.48 \times 10^{-12} + 2.82 \times 10^{-12}}{1.02 \times 10^{-11} + 2.57 \times 10^{-13}} \right) \\ &= 8.6 \times 10^{-3} \text{ cm}^{-1} = 2.2 \times 10^{-2} \text{ in.}^{-1} \approx 0 \end{aligned} \quad (\text{E.9})$$

$$\therefore d\dot{p}a (t, a) = 1.98 \times 10^{-3} e^{-0.22a} + 3.87 \times 10^{-4} (t - 22.6)$$

E.2.1.5 Region 5 (nozzle annulus)

Same as region 4.

E.2.1.6 Region 6 (nozzle weld)

$$d\dot{p}a (100 \text{ MW}, a = 0, t \leq 22.6)$$

$$= (1.72 \times 10^{-13} + 1.95 \times 10^{-12}) \times 3.15 \times 10^7 \quad (\text{E.10})$$

$$= 6.68 \times 10^{-5} / \text{EFPY}(100 \text{ MW})$$

$$\begin{aligned}
 d\dot{p}a(100 \text{ MW}, a = 0, t > 22.6) \\
 &= (1.59 \times 10^{-12} + 2.14 \times 10^{-12}) \times 3.15 \times 10^7 \\
 &= 1.17 \times 10^{-4} / \text{EPY}(100 \text{ MW})
 \end{aligned} \tag{E.11}$$

$$\begin{aligned}
 \lambda(t \leq 22.6) &= \frac{1}{123.2 - 115.8} \ln \left(\frac{1.72 \times 10^{-13} + 1.95 \times 10^{-12}}{1.43 \times 10^{-13} + 3.54 \times 10^{-13}} \right) \\
 &= 0.196 \text{ cm}^{-1} = 0.50 \text{ in.}^{-1}
 \end{aligned}$$

$$\begin{aligned}
 \lambda(t > 22.6) &= \frac{1}{123.2 - 116.8} \ln \left(\frac{1.59 \times 10^{-12} + 2.14 \times 10^{-12}}{1.90 \times 10^{-12} + 3.93 \times 10^{-13}} \right) \\
 &= 0.076 \text{ cm}^{-1} = 0.19 \text{ in.}^{-1}
 \end{aligned}$$

$$\begin{aligned}
 \therefore d\dot{p}a(t \geq 22.6, a, t) &= 1.51 \times 10^{-3} e^{-0.50a} \\
 &\quad + 1.17 \times 10^{-4} (t - 22.6) e^{-0.19a}
 \end{aligned} \tag{E.12}$$

E.2.1.7 Region 7 (nozzle weld)

Same as region 6.

E.2.2 HB-3

As indicated in Fig. E.2, HB-3 is not axisymmetric, and thus there is a variation in $d\dot{p}a$ around the centerline of the nozzle. The maximum values occur at the acute side, and to simplify the probabilistic analysis these values are used for all HB-3 regions. This conservative approach is appropriate because the contribution of HB-3 to the total probability of failure is not dominant.

E.2.2.1 Regions 8, 9, 10 (nozzle corner)

$$\begin{aligned} d\dot{p}a & (100 \text{ MW}, a = 0) \\ &= [4.24 \times 10^{-12} + 1.40 \times 10^{-12}] \times 3.15 \times 10^7 \\ &= 1.78 \times 10^{-4}/EFPY(100 \text{ MW}) \end{aligned} \tag{E.13}$$

$$\lambda \approx 0$$

$$\therefore dpa(t) = 1.78 \times 10^{-4}t. \tag{E.14}$$

E.2.2.2 Regions 11, 12 (nozzle annulus)

Radial location = 5.3 in. = 13.5 cm.

$$\begin{aligned} d\dot{p}a & (100 \text{ MW}, a = 0) \\ &= [1.97 \times 10^{-12} + 1.36 \times 10^{-12}] \times 3.15 \times 10^7 \\ &= 1.05 \times 10^{-4}/EFPY(100 \text{ MW}) \end{aligned} \tag{E.15}$$

$$\lambda \approx 0$$

$$\therefore dpa(t) = 1.05 \times 10^{-4}t. \tag{E.16}$$

E.2.2.3 Regions 13, 14 (nozzle weld)

Radial location = 9.2 in. = 23.4 cm.

$$\begin{aligned} d\dot{p}a & (100 \text{ MW}, a = 0) \\ &= [5.87 \times 10^{-13} + 1.74 \times 10^{-12}] \times 3.15 \times 10^7 \\ &= 7.33 \times 10^{-5}/EFPY(100 \text{ MW}) \end{aligned} \tag{E.17}$$

$$\lambda = \frac{1}{(115.6 - 108.0)} \ln \frac{5.87 \times 10^{-13} + 1.74 \times 10^{-12}}{4.27 \times 10^{-13} + 2.99 \times 10^{-13}}$$

$$= 0.153 \text{ cm}^{-1} = 0.39 \text{ in.}^{-1}$$

$$\therefore dpa(a, t) = 7.3 \times 10^{-5} e^{-0.39at} \quad (E.18)$$

E.2.3 HB-4

HB-4 is treated in the same manner as HB-3 with regard to asymmetry.

3.2.3.1 Regions 15, 16, 17 (nozzle corner)

$$dpa(100 \text{ MW}, t > 22.6, a = 0)$$

$$= [6.98 \times 10^{-12} + 1.83 \times 10^{-12}] 3.15 \times 10^7 \quad (E.19)$$

$$= 2.77 \times 10^{-4} / EFPY(100 \text{ MW})$$

$$dpa(100 \text{ MW}, t \leq 22.6, a = 0)$$

$$= [2.49 \times 10^{-12} + 8.66 \times 10^{-13}] 3.15 \times 10^7 \quad (E.20)$$

$$= 1.06 \times 10^{-4} / EFPY(100 \text{ MW})$$

$$\lambda \approx 0$$

$$\therefore dpa(t) = 1.06 \times 10^{-4} \times 22.6 + 2.77 \times 10^{-4} (t - 22.6) \quad (E.21)$$

$$= 2.4 \times 10^{-3} + 2.8 \times 10^{-4} (t - 22.6)$$

E.2.3.2 Regions 18, 19 (nozzle annulus)

Radial location (same as HB-3) = 13.5 cm.

$$\begin{aligned}
 d\dot{p}a \text{ (100 MW, } t > 22.6, a = 0) \\
 &= [2.35 \times 10^{-12} + 1.29 \times 10^{-12}] 3.15 \times 10^7 \\
 &= 1.15 \times 10^{-4}/EFPY(100 \text{ MW})
 \end{aligned} \tag{E.22}$$

$$\begin{aligned}
 d\dot{p}a \text{ (100 MW, } t \leq 22.6, a = 0) \\
 &= [1.23 \times 10^{-12} + 8.99 \times 10^{-13}] 3.15 \times 10^7 \\
 &= 6.71 \times 10^{-5}/EFPY(100 \text{ MW})
 \end{aligned} \tag{E.23}$$

$$\begin{aligned}
 \lambda &\approx 0 \\
 \therefore d\dot{p}a(t) &= 6.71 \times 10^{-5} \times 22.6 + 1.15 \times 10^{-4} (t - 22.6) \\
 &= 1.52 \times 10^{-3} + 1.15 \times 10^{-5} (t - 22.6)
 \end{aligned} \tag{E.24}$$

E.2.2.3 Regions 20, 21 (nozzle weld)

Radial location = 23.4 cm.

$$\begin{aligned}
 d\dot{p}a \text{ (100 MW, } t > 22.6, a = 0) \\
 &= [6.16 \times 10^{-13} + 9.72 \times 10^{-13}] 3.15 \times 10^7 \\
 &= 5.00 \times 10^{-5}/EFPY(100 \text{ MW})
 \end{aligned} \tag{E.25}$$

$$\begin{aligned}
 d\dot{p}a \text{ (100 MW, } t \leq 22.6, a = 0) \\
 &= [4.62 \times 10^{-13} + 1.21 \times 10^{-12}] 3.15 \times 10^7 \\
 &= 5.27 \times 10^{-5}/EFPY(100 \text{ MW})
 \end{aligned} \tag{E.26}$$

$$\lambda(t > 22.6) = \frac{1}{110.5 - 101.6} \ln \frac{6.16 \times 10^{-13} + 9.72 \times 10^{-13}}{3.75 \times 10^{-13} + 1.12 \times 10^{-13}}$$

$$= 0.133 \text{ cm}^{-1} = 0.34 \text{ in.}^{-1}$$

$$\lambda(t < 22.6) = \frac{1}{110.5 - 101.6} \ln \frac{4.62 \times 10^{-13} + 1.21 \times 10^{-12}}{2.34 \times 10^{-13} + 1.18 \times 10^{-13}}$$

$$= 0.175 \text{ cm}^{-1} = 0.44 \text{ in.}^{-1}$$

$$\therefore dpa(t, a) = 5.27 \times 10^{-5} \times 22.6 \times e^{-0.44a} + 5.00 \times 10^{-5} (t - 22.6) e^{0.34a}$$

$$= 1.19 \times 10^{-3} e^{-0.44a} + 5.0 \times 10^{-5} (t - 22.6) e^{-0.34a}$$
(E.27)

E.2.4 HB-1

The present design of HB-4 is the same as that for HB-1. Thus, dpa values for HB-1 are the same as those derived for the present design of HB-4 in Sect. E2.3 ($t \leq 22.6$).

E.2.4.1 Regions 22, 23, 24 (nozzle corner)

$$dpa(t) = 1.06 \times 10^{-4} t$$
(E.28)

E.2.4.2 Regions 25, 26 (nozzle annulus)

$$dpa(t) = 6.7 \times 10^{-5} t$$
(E.29)

E.2.4.3 Regions 27, 28 (nozzle weld)

$$dpa(t, a) = 5.3 \times 10^{-5} e^{-0.44at}$$
(E.30)

E.2.5 Region 29 (vessel circumferential weld below core)

Data taken from Ref. 24 RZ calculation of "core" (no beam tubes).

Axial location = 17 in. (43.2 cm) below core HMP.

$$dpa(100 \text{ MW}, a = 0)$$

$$= [5.0 \times 10^{-14} + 7.3 \times 10^{-13} + 4.3 \times 10^{-13}] 3.15 \times 10^7 \quad (E.31)$$

$$= 3.8 \times 10^{-5} / \text{EPY}(100 \text{ MW})$$

$$\lambda = \frac{1}{125.9 - 119.5} \ln \frac{5.0 \times 10^{-14} + 7.3 \times 10^{-13} + 4.3 \times 10^{-13}}{2.0 \times 10^{-14} + 1.53 \times 10^{-13} + 8.0 \times 10^{-14}}$$

$$= 0.24 \text{ cm}^{-1} = 0.62 \text{ in.}^{-1}$$

$$\therefore dpa(t, a) = 3.8 \times 10^{-5} e^{-0.62at} \quad (E.32)$$

E.2.6 Region 30 (vessel axial weld)

Axial location: core HMP

$$dpa(100 \text{ MW}, a = 0)$$

$$= [9.5 \times 10^{-14} + 1.02 \times 10^{-12} + 6.0 \times 10^{-13}] 3.15 \times 10^7 \quad (E.33)$$

$$= 5.4 \times 10^{-5} / \text{EPY}(100 \text{ MW})$$

$$\lambda = \frac{1}{125.9 - 119.5} \ln \frac{9.5 \times 10^{-14} + 1.02 \times 10^{-12} + 6.0 \times 10^{-13}}{3.8 \times 10^{-14} + 2.17 \times 10^{-13} + 1.02 \times 10^{-13}}$$

$$= 0.25 \text{ cm}^{-1} = 0.63 \text{ in.}^{-1}$$

$$\therefore dpa(t, a) = 5.4 \times 10^{-5} e^{-0.63at} \quad (E.34)$$

E.2.7 Region 31 (vessel shell in beltline)

Same as region 30.

$$\therefore dpa(t, a) = 5.4 \times 10^{-5} e^{-0.63at} \quad (E.35)$$

HFIR hb2 (4 in. diameter)

Fig. E.1. HB-2 (present design) 2-D model.

HFIR Geometry (hfir 9 in. beam tube from RC drawing)

Fig. E.2. HB-2 (new design) 2-D model.

Fig. E.3. HB-3 (present design) 3-D model, horizontal cross section.

Fig. E.5. HB-4 (new design) 3-D model, horizontal cross section.

Fig. E.6. HFIR core 2-D (R-Z) model with neutron iso dpa.

HFIR fs 84gp 100MW (unnormalized) (total p+d) g-dpa

Fig. E.7. HFIR core 2-D (R-Z) gamma iso *dpa*.

Fig. E.8. HFIR core 2-D (R-Z) total ($n+\gamma$) iso dpa.

HFIR hb2 4 in., 100 MW, 84 gp, n-dpa (normalized)

Fig. E.9. HB-2 (present design) neutron iso dpa.

HFIR hb2 4 in., 100 MW, 84 gp, g-dpa (normalized)

Fig. E.10. HB-2 (present design) gamma iso dpa.

HFIR hb2 4 in., 100 MW, 84 gp, total dpa (normalized)

Fig. E.11. HB-2 (present design) total ($\eta+\gamma$) iso dpa.

HFIR hb2 9 in., 100 MW, 34 gp, n-gas

Fig. E.12. HB-2 (new design) neutron iso dpa.

HFIR hb2 9 in., 100 MW, 84 gp, g-dpa (normalized)

Fig. E.13. HB-2 (new design) gamma iso dpa.

HFIR hb2 9 in., 100 MW, 84 gp, total dpa (normalized)

Fig. E.14. HB-2 (new design) total ($n+\gamma$) iso dpa.

HFIR HB3 Z cut neutron dpa (no fs in fuel)(normalized)

Fig. E.15. HB-3 neutron iso dpa in horizontal midplane.

Fig. E.16. HB-3 gamma iso dpa in horizontal midplane.

Fig. E.17. HB-3 total ($\eta+\gamma$) iso dpa in horizontal midplane.

Fig. E.18. HB-4 (present design) neutron iso dpa in horizontal midplane.

Fig. E.19. HB-4 (present design) gamma iso dpa in horizontal midplane.

Fig. E.20. HB-4 (present design) total ($\eta+\gamma$) iso dpa in horizontal midplane.

Fig. E.21. HB-4 (new design) neutron iso $\dot{d}pa$ in horizontal midplane.

Fig. E.22. HB-4 (new design) gamma iso dpa in horizontal midplane.

HFIR HB4 Z cut total dpa (no fs in fuel)(normalized)

Fig. E.23. HB-4 (new design) total ($\gamma+\gamma$) iso dpa.

Fig. E.24. dpa vs beam tube diameter at HB-2 nozzle corner.

APPENDIX F

STRESSES USED FOR EACH OF THE VESSEL REGIONS

F.1 Stresses Required

The stresses required as input to the calculation of stress intensity factors are the stresses normal to the plane of the postulated crack. Stresses included are those due to pressure, welding (residual stresses in welds), and differential thermal expansion between cladding and base material. Discontinuity stresses associated with the beam-tube nozzles were obtained by performing three-dimensional linear-elastic finite-element analyses specifically for the HB-2 and HB-3 nozzles.²⁵

F.2 Calculation of Region Stresses

Following weld-deposit application of the cladding over weld and nozzle areas and the post-weld heat treatment of the vessel, the cladding residual stress at room temperature is calculated to be essentially the "yield" value. Based on cladding-related data in Refs. 34, 35, and 36, a value of 25 ksi was used for the HFIR analysis for all regions, although stresses in the roll-bond portion of the cladding may be less. A compensating stress of -1 ksi was applied to the base material. Other stresses are discussed below on a per-region basis.

F.2.1 HB-2

F.2.1.1 HB-2 nozzle corner

Stresses used for calculating K_I for nozzle corner flaws are the nominal pressure membrane stresses normal to the flaw plane and the differential thermal expansion stress in the inner-surface cladding:

Pressure stresses:

$$\sigma_1 = p \frac{r_v}{w} = \frac{48}{3} p = 16p \quad (F.1)$$

$$\sigma_2 = \frac{pr_v}{2w} = 8p \quad (F.2)$$

$$\sigma_3 \approx \frac{pr_v}{w} \cos^2 45^\circ + \frac{pr_v}{2w} \cos^2 45^\circ = 12p \quad (F.3)$$

where

r_v = mean radius of vessel
 w = vessel wall thickness

F.2.1.2 Annular region between nozzle-corner region and nozzle weld (regions 4 and 5)

Based on the results in Ref. 25;

Axial flow above nozzle (region 4):

$$\sigma_4 = 5(6-a)p, \quad (F.4)$$

where

a = depth in vessel wall (in.).

Circumferential flow at horizontal midplane (region 5):

$$\sigma_5 = \frac{1}{2} \sigma_4 = 2.5(6-a)p \quad (F.5)$$

F.2.1.3 Nozzle weld (regions 6 and 7)

For these regions, the pressure-induced and residual stresses were taken from Ref. 25 and 5, respectively.

Axial flow above nozzle (region 6):

From Ref. 2

$$\sigma = p \left[\left(\frac{\sigma_m}{p} \right) + \left(\frac{\sigma_b}{p} \right) \left(1 - \frac{2a}{w} \right) \right] + \sigma_r, \quad (F.6)$$

where

σ_m = membrane stress

σ_b = bending stress

σ_r = residual stress

p = primary-system pressure

$$\sigma_6 = p \left[17.5 + 5.9 \left(1 - \frac{2a}{w} \right) \right] + 8.5, \quad (F.7)$$

Circumferential flow at horizontal midplane (region 7):

$$\sigma_7 = p \left[8.1 - 3.9 \left(1 - \frac{2a}{w} \right) \right] + 8.5 \quad (F.8)$$

Axial flow at horizontal midplane (region 7):

$$\sigma_7 = p \left[11.3 - 4.5 \left(1 - \frac{2a}{w} \right) \right] + 1.7 \quad (F.9)$$

The maximum pressure considered in the probabilistic analysis is $p = p(HT) = 900$ psi. For this case,

$$\sigma_7(\text{cir. flaw, } a = 1.5) = 7.3 + 8.5 = 15.8 \text{ ksi} \quad (F.10)$$

$$\sigma_7(\text{axial flaw, } a = 1.5) = 11.3 + 1.7 = 13.0 \text{ ksi} \quad (F.11)$$

This comparison indicates that the circumferential flaw [Eq. (F.8)] should be used in the probabilistic analysis.

F.2.2 HB-3

HB-3 is not axisymmetric but is treated as if it were, using the regions with the highest stresses, whether in the same quadrant or not.

F.2.2.1 Nozzle corner (regions 8, 9, and 10)

$$\sigma_8 = \frac{pr_v}{w} = \frac{48}{3} p = 16p \quad (F.12)$$

$$\sigma_9 = \frac{pr_v}{2w} = 8p \quad (F.13)$$

$$\sigma_{10} \cong 12p \quad (F.14)$$

F.2.2.2 Annual region between nozzle-corner region and nozzle weld (regions 11 and 12)

These stresses are essentially the same as for HB-2.

Axial flow above nozzle (region 11):

$$\sigma_{11} = 5(6 - a)p \quad (F.15)$$

Circumferential flaw at horizontal midplane (region 12):

$$\sigma_{12} = 2.5(6 - a)p \quad (F.16)$$

F.2.2.3 Nozzle weld (regions 13 and 14)

$$\sigma_{13} = p \left[21.4 + 1.8 \left(1 - \frac{2a}{w} \right) \right] + 8.5 \quad (F.17)$$

Circumferential flaw at horizontal midplane (region 14):

$$\sigma_{14} = p \left[8.3 - 2.5 \left(1 - \frac{2a}{w} \right) \right] + 8.5 \quad (F.18)$$

F.2.3 HB-1 and HB-4 (regions 15–28)

Stresses for HB-1 and HB-4 are assumed to be the same as for HB-3.

F.2.4 Vessel circumferential weld below core (region 29)

An axial flaw is exposed to both the hoop stress and the maximum residual stress.

$$\sigma_{29} = \frac{pr_v}{w} + \sigma_r = p \times \frac{48}{3} + 8.5 = 16p + 8.5 \quad (F.19)$$

F.2.5 Vessel axial weld (region 30)

Axial flaw:

Residual stresses normal to the length of weld are essentially zero because of the flexibility of the vessel wall. Thus,

$$\sigma_{30A} = \frac{pr_v}{w} = p \times \frac{48}{3} = 16p \quad (F.20)$$

Circumferential flaw:

$$\sigma_{30C} = \frac{pr_v}{2w} + \sigma_r = 8p + 8.5 \quad (F.21)$$

For $p = p(HT) = 900$ psi,

$$\sigma_{30A} = 16 \times 900 = 14.4 \text{ ksi}$$

$$\sigma_{30C} = 8 \times 900 + 8.5 = 15.7 \text{ ksi}$$

Thus,

$$\sigma_{30} = 8p + 8.5 \quad (F.22)$$

F.2.6 Vessel shell (region 31)

$$\sigma_{31} = \frac{pr_v}{w} = p \times \frac{48}{3} = 16p \quad (F.23)$$

APPENDIX G

STRESS INTENSITY FACTORS FOR FLAWS IN THE VARIOUS VESSEL REGIONS

G.1 Method of Analysis

Stress intensity factors (K_I) for hypothetical flaws in the HFIR vessel are the result of stresses due to internal pressure, cladding differential thermal expansion, and, in weld areas, residual stress due to welding.

For all regions but the nozzle corners, K_I values are calculated using influence coefficients, as described in Refs. 15, 16, and 17. The necessary coefficients are included in Ref. 12, and the stresses required are the through-wall stresses calculated in Appendix F.

For the nozzle-corner flaw, K_I due to pressure and cladding is

$$K_I = [F(a/r_i)_p \sigma_p + F(a/r_i)_c \sigma_c] \sqrt{\pi a}, \quad (G.1)$$

where

a	=	flaw dimension (Fig. B.1)
$F(a/r_i)_p$	=	geometry factor for pressure loading
$F(a/r_i)_c$	=	geometry factor for cladding differential-expansion load
σ_p	=	pressure-induced nominal principal stress normal to flaw plane
σ_c	=	nominal stress in cladding due to differential thermal expansion

Values of $F(a/r_i)$ were obtained from 3-D finite-element fracture analyses performed for the HB-2 nozzle with cladding and an axially-oriented nozzle-corner flaw. Values of K_I as a function of flaw size and position on the flaw front were calculated first (Figs. G.1 and G.2),¹⁸ and then values of $F(a/r_i)$ were calculated from

$$F(a/r_i) = K_I / \sigma \sqrt{\pi a} \quad (G.2)$$

As indicated in the figures, the maximum values of K_I occur either at the inner surface or at the clad/base interface. As mentioned earlier (Sect. 4.3.2), the potential for flaw propagation in cladding is very small because of the relatively high toughness of the cladding. Thus, $F(a/r_i)$ was calculated at the clad base interface only. Values of K_I and corresponding values of $F(a/r_i)$ are included in Table G.1. Equations fit to the data are as follows:

$$F(a/r_i)_p = 3.24 - 2.97(a/r_i) \quad (G.3)$$

$$F(a/r_i)_c = 0.420 - 2.01(a/r_i) + 4.18(a/r_i)^2 \quad (G.4)$$

These equations can be used for the other nozzles by using the appropriate values of r_i . For HB-2,

$$a/r_i = a/7 = 0.143a \text{ in.}^{-1}, \quad (G.5)$$

Table G.1. $K_I(p)$ and $K_I(c)$ for an axially-oriented nozzle-corner flaw of circular front in the HB-2 nozzle ($r_i = 7.0$ in.) with $\sigma_p = 14.4$ ksi and $\sigma_c = 25$ ksi, and the corresponding values of $F(a/r_i)$

Flaw radius ^a in.	a in.	a/r_i	$K_I(p)$ ksi $\sqrt{\text{in.}}$	$K_I(c)$ ksi $\sqrt{\text{in.}}$	$F(a/r_i)_p$	$F(a/r_i)_c$
0.25	0.209	0.0298	36.5	7.4	3.13	0.366
0.50	0.397	0.0566	49.9	8.9	3.10	0.319
0.75	0.647	0.0924	60.3	9.6	2.94	0.269
1.00	0.793	0.1133	66.3	9.7	2.92	0.246
1.25	1.043	0.1490	72.6	9.8	2.79	0.217
1.50	1.293	0.1847	78.0	9.8	2.69	0.194
	1.500 ^b	0.2143	81.3	9.8	2.60	0.181

^aSee Fig. B.1.

^bExtrapolated value.

and for HB-1, HB-3, and HB-4,

$$a/r_i = a/4 = 0.250a \text{ in.}^{-1} \quad (G.6)$$

G.2 Calculation of K_I

G.2.1 HB-2

G.2.1.1 Nozzle corner (regions 1, 2, and 3)

Equations (G.1) and (G.4) are used for calculating K_I for each of the three regions, and the following values of $F(a/r_i)_p$ and σ_p are used:

Axial nozzle-corner flaw at top or bottom of nozzle (region 1):

$$F(a/r_i)_p: \text{ Eq. (G.3)}$$

$$\sigma_p: \text{ Eq. (F.1)}$$

Circumferential nozzle-corner flaw 90° from top of nozzle (region 2):

As indicated by the stress distribution in Ref. 25,

$$F(a/r_i)_p: \approx 1.0$$

$$\sigma_p: \text{Eq. (F.2)}$$

Flaw normal to the principal stress at 45° to the top or bottom of the nozzle (region 3):

The value of $F(a/r_i)$ at the top of the nozzle for $a/r_i = 0$ is approximately equal to the stress intensification at the nozzle corner. At 45° the stress intensification is about the same (somewhat less) as at 0° when based on the nominal principle stress at 45°. Thus, Eq. (G.3) is appropriate at 45°, if Eq. (F.3) is used for σ_p in Eq. (G.2), and it is.

G.2.1.2 Annular region between nozzle-corner region and nozzle weld (regions 4 and 5)

The assumed flaws are 6:1 semielliptical cracks normal to the maximum principle stresses at 0° and 90°. Values of K_t due to pressure and cladding are calculated using appropriate influence coefficients included in Ref. 12 and pressure-induced stresses from Ref. 25. The appropriate stresses are given by Eqs. (F.4) and (F.5).

G.2.1.3 Nozzle weld (regions 6 and 7)

K_t values are calculated by the same method as described in G.2.1.2. The appropriate pressure-induced and welding residual stresses are given by Eqs. (F.7) and (F.8).

G.2.2 HB-3 (regions 8–14)

The same procedures are used for HB-3 as for HB-2, and the appropriate stresses are given by Eqs. (F.12) through (F.18).

G.2.3 HB-1 and HB-4 (regions 15–28)

The same procedures and stresses are used for HB-1 and HB-4 as for HB-3.

G.2.4 Vessel circumferential weld below core (region 15)

The assumed flaw is an axial 6:1 semielliptical crack. K_t values are calculated using influence coefficients. The pressure-induced and welding residual stresses are given by Eq. (F.19).

G.2.5 Vessel axial weld (region 30)

The assumed flaw is a circumferential 6:1 semielliptical crack. K_t values are calculated using influence coefficients. The pressure-induced and welding residual stresses are given by Eq. (F.22).

G.2.6 Vessel shell (region 31)

The assumed flaw is an axial 6:1 semielliptical crack. K_I values are calculated using influence coefficients. The pressure-induced stresses are given by Eq. (F.23).

HFIR HB-2 NOZZLE ANALYSIS: 0.25-1.5 inch radii corner flaws

Fig. G.1. Stress intensity factors for part-circular axially oriented, nozzle-corner flaw at top of HB-2 nozzle.

HFIR HB-2 NOZZLE ANALYSIS: 0.5-1.5 inch radii corner flaws

Fig. G.2. Stress intensity factors for part-circular, axially oriented, nozzle-corner flaw at top of HB-2 nozzle, with and without cladding effect.

APPENDIX H

CALCULATION OF FLAW GROWTH BY FATIGUE FOR THE HB-2 NOZZLE CORNER

H.1 Method of Analysis

The analytical procedure for calculating flaw growth by fatigue is included in Section XI of the ASME Code.¹⁹ In that document, flaw growth per load cycle (da/dN) is calculated from

$$\frac{da}{dN} = m Q_2 \Delta K^n, \quad (H.1)$$

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 = $kR + 1$ 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

For the HB-2 nozzle corner

$$K_{min} = 0$$

$$K_{max} = F(\hat{a}/r_n) \sigma_h \sqrt{\pi a}, \quad (\text{Ref. 5}) \quad (H.2)$$

$$F(\hat{a}/r_n) = 2.5 - 5.13(\hat{a}/r_n) + 5.63(\hat{a}/r_n)^2, \quad (\hat{a}/r_n \leq 0.40) \quad (H.3)$$

$$a/r_n = 0.137a \text{ in}^{-1} \quad (H.4)$$

where

- r_n = "nozzle" radius to "center" of flaw (refer to Fig. B.1)
- σ_h = far-field stress
- $= \frac{p\bar{R}}{w}$ (H.5)
- p = primary-system pressure
- $\bar{R}$ = average radius of vessel wall
- w = thickness of vessel wall

Equation H.1 is plotted in Fig. A-4300-1 of Ref. 19 and is included herein as Fig. H.1. As indicated in Fig. H.1, 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

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

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

As will be demonstrated later, the value of ΔK is such that the upper portion of the bilinear curve in Fig. H.1 is appropriate for the problem at hand. Thus,

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

Equation H.7 must be multiplied by an average value of dN/dt to obtain the crack growth per unit of time (da/dt):

$$\frac{da}{dt} = \frac{da}{dN} \times \frac{dN}{dt}, \quad (H.8)$$

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

H.2 Calculations

The load cycle of concern is pressurization/depressurization associated with normal startup and shutdown, in which case $p = 468$ psi. The hydro test does not contribute significantly because of the relatively few cycles.¹ Thus,

$$\sigma_h \approx 16p = 16 \times 468 = 7.5 \text{ ksi}$$

Conservatively assuming $a = 1.0$ in.,

$$a/r_n = 0.137 \times 1 = 0.137$$

$$F(a/r_n) = 2.5 - 5.13(0.137) + 5.63(0.137)^2 = 1.90$$

$$K_{\max} = 1.90 \times 7.5 \sqrt{\pi \times 1} = 25 \text{ ksi } \sqrt{\text{in.}}$$

$$K_{\min} = 0$$

$$R = \frac{K_{\min}}{K_{\max}} = \frac{0}{25} = 0$$

$$\Delta K_I = K_{\max} - K_{\min} = 25 - 0 = 25 \text{ ksi } \sqrt{\text{in.}}$$

$$\frac{da}{dN} = 1.01 \times 10^{-1} \Delta K^{1.95} = 1.01 \times 10^{-1} (25)^{1.95} = 54 \times 10^{-6} \text{ in./cycle}$$

$$da/dt = 22 \text{ cycles/year, (Ref.1)}$$

$$\therefore \dot{a} = 54 \times 10^{-6} \times 22 = 1.2 \times 10^{-3} \text{ in./year}$$

Fig. H.1. Reference fatigue crack growth curves for carbon and low alloy ferritic steels (Ref. 19).

APPENDIX I

DETAILED RESULTS OF $P(F/E)$ CALCULATIONS

I.1 Results Without Beneficial Effect of Hydro Testing

Results of the $P(F/E)$ calculations are included in Tables I.1, I.2, and I.3, and Figs. I.1, I.2, and I.3 for the hydro-test condition and in Tables I.4 and I.5, and Figs. I.5 and I.6 for the emergency/faulted (E/F) condition. These tables and figures indicate the contribution to $P(F/E)$ of each of the 31 vessel regions calculated. For a region not listed, $P(F/E) < 10^{-9}$.

Table I.3 includes for region 1 (HB-2 nozzle corner) the number of flaws of a given size that results in vessel failure for the hydro-test loading condition. As indicated, at $t = 50$ EFPY (100 MW), only 8% of the failures are with the smallest (0.125 in.) flaw. This further justifies the exclusion of flaws that reside entirely within the cladding.

Figure I.3 is a histogram of region 1 RT_{NDT} values that result in failures. The most likely value is 212°F , and the corresponding value of $T_v - RT_{NDT}$ is -127°F . With reference to Fig. I.4, which is taken from Ref. 38, is apparent that for this value of $T - RT_{NDT}$ there are many K_{Ic} data points. This lends credence to the K_{Ic} distribution curve used in the calculation of $P(F/E)$.

I.2 Results with Beneficial Effect of Hydro Testing

The above results do not include the beneficial effect of hydro-testing on $P(F/E)$, but the effect can be included as discussed in Sects. 4.2 and 4.4.2 and Ref. 10. Calculations of the effect are included below.

Immediately following a successful hydro test $P(F/E) = 0$. Just prior to the next hydro test,

$$P(F/E)_{\max,n} = P(F/E)_n - P(F/E)_{n-1}, \quad (I.1)$$

where

$$\begin{aligned} P(F/E)_n &= \text{uncorrected } P(F/E) \text{ just prior to the second hydro test} \\ P(F/E)_{n-1} &= \text{uncorrected } P(F/E) \text{ for preceding hydro test} \\ P(F/E)_{\max,n} &= \text{corrected value of } P(F/E) \text{ just prior to second hydro test} \end{aligned}$$

Values of $P(F/E)_n$ and $P(F/E)_{\max,n}$ and the corresponding values of $t(HT)$ and Δt are given in Table I.6. These results are discussed in Sect. 4.4.2.

The beneficial effect of hydro testing can also be applied in the calculation of $P(F/E)$ for the E/F condition. When the above approach is used, $P(F/E)_n$ corresponds to the $P(F/E)(E/F)$ curve in Fig. 5, and $P(F/E)_{n-1}$ corresponds to the $P(F/E)(HT)$ curve in Fig. 5. Thus, depending on the value of Δt , $P(F/E)_{\max,n}$ could be negative. For example, suppose $t(HT) = 28.6$ EFPY (100 MW), and $\Delta t = 3$ EFPY (100 MW). Then

$$P(F|E)(E/F)_{\max,n} = 3.00 \times 10^{-6} - 4.60 \times 10^{-6} = -1.6 \times 10^{-6}.$$

If $\Delta t = 39.6 - 28.6 = 11.0$ EFPY(100 MW),

$$P(F|E)(E/F)_{\max,n} = 1.10 \times 10^{-5} - 1.10 \times 10^{-5} = 0.$$

Because the specified values of Δt are less than 11.0 EFPY(100 MW), the calculated probability of failure for the E/F condition, taking advantage of the beneficial effect of hydro testing, will always be zero (negative values imply zero probability).

Another and conservative approach to applying the beneficial effect of hydro testing in the calculation of $P(F/E)(E/F)$ is to specify, for the purpose of the calculation, hydro test conditions identical to the E/F conditions. Then Eq. (E.1) can be applied to the $P(F/E)(E/F)$ curve in Fig. 5 without involving the $P(F/E)(HT)$ curve. This was done for the Δt values included in Table I.1 and the results are given in Table I.7. The results are discussed in Sect. 4.4.2.

I.3 Derivation of Maximum Permissible Time Between Hydro Tests, Based on $P(F/E)$ Analysis

As mentioned in Sect. 3 and Appendix A, the maximum permissible time between hydro tests can be calculated using both deterministic and probabilistic methods. For both methods, it is specified that the potential for vessel failure during the hydro test must be equal to or greater than that for the E/F conditions, "all" uncertainties accounted for. Both approaches are discussed in Appendix A, and additional discussion is provided here for the probabilistic approach.

"Equal" potential for the probabilistic approach is defined as

$$P(F/E)(HT) \geq P(F/E)(E/F) \quad (I.2)$$

For the case in which the beneficial effect of hydro testing is not included in the calculation of $P(F/E)$, the maximum permissible time between hydro tests (Δt) is the difference in times corresponding to equal values of $P(F/E)(HT)$ and $P(F/E)(E/F)$, using the $P(F/E)$ vs t curves in Fig. 5. Thus,

$$\Delta t_{\min} = 30.0 - 23.5 = 6.3 \text{ EFPY}(100\text{MW})$$

As time progresses, the value of Δt increases.

Uncertainties that affect the relative values of the two curves in Fig. 5 and that are not included in the probabilistic analysis (uncertainties in the loads and in the distributions for dpa , $\Delta NDTT$, and K_{IC} , each of which is considered to be small) are accounted for by applying a safety factor of 0.5 to Δt as calculated above. Thus, based on the probabilistic approach, and without other considerations, the minimum value of Δt is 3 EFPY(100 MW).

If the beneficial effect of hydro testing were included in the calculation of $P(F/E)$, the minimum value of Δt would be greater because for $\Delta t = 3.0$ EFPY(100 MW), $P(F/E)(HT) \neq 0$, and $P(F/E(E/F)) = 0$ for all values of t . However, as mentioned in Sect. 4.4.2, for $P(F/E)(HT)_{\max}$ to satisfy the maximum permissible value of 1×10^{-5} , Δt must be no greater than 3 EFPY(100 MW).

As indicated in Sect. 3, the initial value of Δt based on the deterministic method is only 0.75 EFPY(100 MW), and for $t(HT) = 33$ EFPY(100 MW) it is still only 3.4 EFPY(100 MW). Thus, the deterministic approach results in relatively conservative values.

The large difference in Δt values obtained with the deterministic and probabilistic approaches is associated with the application, in the deterministic approach, of "necessary" conservatism regarding the initial toughness of the material and the uncertainty in the damage rate. Neither of these conservatisms are necessary in the probabilistic approach.

The more conservative approach has been used for present operating conditions because it resulted in reasonable values of Δt , other things being considered. However, the values of Δt calculated for the proposed new design are small enough, perhaps, to impact operations. Based on the probabilistic approach, it is reasonable to let $\Delta t = 3.0$ EFPY(100 MW) for all hydro tests beginning at $t = 22.6$ EFPY(100 MW).

Table I.1. $P(F/E)$ vs t for nozzle-corner regions:
hydro-test conditions, upgrade design

C:\HFIR\HB234R1\LOAD2\PNOZZ.DAT (11-19-98)

	-1- EFPY	-2- reg 1	-3- reg 2	-4- reg 3	-5- reg 8	-6- reg 9
1	22.6000	8.5000e-9	0.0000		1.5000e-7	0.0000
2	25.0000	6.2000e-7	0.0000	4.4000e-8	2.4000e-7	0.0000
3	27.5000	3.4000e-6	0.0000	3.4000e-7	3.4000e-7	0.0000
4	30.0000	8.2000e-6	0.0000	1.0000e-6	4.8000e-7	0.0000
5	35.0000	1.9000e-5	0.0000	3.1000e-6	7.6000e-7	0.0000
6	40.0000	2.8000e-5	0.0000	5.2000e-6	1.2000e-6	0.0000
7	45.0000	3.4000e-5	0.0000	6.8000e-6	1.6000e-6	0.0000
8	50.0000	3.7000e-5	0.0000	7.9000e-6	2.2000e-6	0.0000
9	55.0000	3.9000e-5	0.0000	8.8000e-6	2.5000e-6	0.0000

	-7- reg 10	-8- reg 15	-9- reg 16	-10- reg 17	-11- reg 22	-12- reg 23
1	4.0000e-9	2.4000e-7	0.0000	5.0000e-9	2.4000e-7	0.0000
2	1.2000e-8	5.1000e-7	0.0000	3.5000e-8	3.3000e-7	0.0000
3	2.3000e-8	8.7000e-7	0.0000	6.9000e-8	4.2000e-7	0.0000
4	2.9000e-8	1.4000e-6	0.0000	1.2000e-7	5.7000e-7	0.0000
5	6.1000e-8	2.5000e-6	0.0000	2.6000e-7	8.3000e-7	0.0000
6	9.4000e-8	3.7000e-6	0.0000	4.0000e-7	1.2000e-6	0.0000
7	1.4000e-7	4.9000e-6	0.0000	5.9000e-7	1.6000e-6	0.0000
8	2.1000e-7	6.1000e-6	0.0000	7.9000e-7	2.0000e-6	0.0000
9	2.7000e-7	6.7000e-6	0.0000	9.7000e-7	2.4000e-6	0.0000

	-13- reg 24
1	5.0000e-9
2	2.1000e-8
3	2.9000e-8
4	3.8000e-8
5	6.8000e-8
6	9.6000e-8
7	1.4000e-7
8	1.9000e-7
9	2.4000e-7

Table I.2. $P(F/E)$ vs t for non-nozzle-corner regions:
hydro-test conditions, upgrade design

C:\HFIR\HB234R1\LOAD1\PFEX.DAT (11-19-88)

	-1- EFPY	-2- reg 4	-3- reg 6	-4- reg 11	-5- reg 13	-6- reg 18
1	22.6000		2.8000e-7		3.8000e-7	1.0000e-9
2	25.0000		4.4000e-7		4.8000e-7	1.3000e-9
3	27.5000		6.5000e-7		6.1000e-7	1.6000e-9
4	30.0000	1.5000e-9	9.1000e-7	1.5000e-9	6.9000e-7	1.9000e-9
5	35.0000	1.6000e-8	1.7000e-6	7.0000e-9	9.7000e-7	2.5000e-9
6	40.0000	5.4000e-8	2.5000e-6	8.5000e-9	1.4000e-6	3.3000e-9
7	45.0000	1.3000e-7	3.5000e-6	1.5000e-8	1.7000e-6	3.9000e-9
8	50.0000	2.6000e-7	4.6000e-6	2.4000e-8	2.1000e-6	4.5000e-9
9	55.0000	4.3000e-7	5.8000e-6	2.6000e-8	2.5000e-6	5.5000e-9

	-7- reg 20	-8- reg 25	-9- reg 27	-10- reg 29	-11- reg 31
1	1.8000e-7	1.0000e-9	1.8000e-7	7.8000e-7	0.0000
2	2.3000e-7	3.0000e-9	2.3000e-7	8.4000e-7	0.0000
3	2.9000e-7	4.0000e-9	2.9000e-7	9.2000e-7	0.0000
4	3.1000e-7	5.0000e-9	3.1000e-7	1.0000e-6	0.0000
5	4.4000e-7	1.3000e-8	4.3000e-7	1.2000e-6	0.0000
6	6.1000e-7	1.6000e-8	6.0000e-7	1.5000e-6	0.0000
7	8.0000e-7	2.9000e-8	7.8000e-7	1.7000e-6	0.0000
8	1.0000e-6	3.7000e-8	9.9000e-7	2.0000e-6	0.0000
9	1.3000e-6	4.0000e-8	1.3000e-6	2.2000e-6	0.0000

Table I.3. $P(F/E)$ vs t for nozzle-corner regions:
emergency/faulted conditions, upgrade design

C:\HFIR\HB234R1\LOAD2\PNOZZ.DAT

	-1- EFPY	-2- reg 1	-3- reg 2	-4- reg 3	-5- reg 8	-6- reg 9
1	22.6000		0.0000		9.0000e-9	0.0000
2	25.0000	9.3000e-8	0.0000	4.0000e-9	3.2000e-8	0.0000
3	27.5000	6.9000e-7	0.0000	3.9000e-8	4.0000e-8	0.0000
4	30.0000	1.9000e-6	0.0000	1.5000e-7	6.2000e-8	0.0000
5	35.0000	5.3000e-6	0.0000	5.1000e-7	1.0000e-7	0.0000
6	40.0000	8.1000e-6	0.0000	9.6000e-7	1.9000e-7	0.0000
7	45.0000	1.1000e-5	0.0000	1.3000e-6	2.7000e-7	0.0000
8	50.0000	1.2000e-5	0.0000	1.6000e-6	3.6000e-7	0.0000
9	55.0000	1.3000e-5	0.0000	1.8000e-6	4.8000e-7	0.0000

	-7- reg 10	-8- reg 15	-9- reg 16	-10- reg 17	-11- reg 22	-12- reg 23
1		1.9000e-8	0.0000	1.0000e-9	1.9000e-8	0.0000
2		6.8000e-8	0.0000	2.0000e-9	4.2000e-8	0.0000
3		1.3000e-7	0.0000	3.0000e-9	5.4000e-8	0.0000
4		2.4000e-7	0.0000	1.2000e-8	7.5000e-8	0.0000
5	8.0000e-9	4.3000e-7	0.0000	2.7000e-8	1.1000e-7	0.0000
6	9.5000e-9	7.2000e-7	0.0000	4.0000e-8	2.0000e-7	0.0000
7	1.5000e-8	1.0000e-6	0.0000	6.6000e-8	2.6000e-7	0.0000
8	1.6000e-8	1.3000e-6	0.0000	8.9000e-8	3.4000e-7	0.0000
9	2.8000e-8	1.5000e-6	0.0000	1.2000e-7	4.4000e-7	0.0000

	-13- reg 24
1	
2	
3	1.5000e-9
4	2.5000e-9
5	8.5000e-9
6	9.5000e-9
7	1.5000e-8
8	1.6000e-8
9	2.3000e-8

Table I.4. P(F|E) vs t for non-nozzle-corner regions:
emergency/faulted conditions, upgrade design

C:\HFIR\HB234R1\LOAD2\PFEAX.DAT

	-1- EFPY	-2- reg 4	-3- reg 6	-4- reg 11	-5- reg 13	-6- reg 18
1	22.6000		3.7000e-8		7.0000e-8	0.0000
2	25.0000		7.2000e-8		8.7000e-8	0.0000
3	27.5000		9.9000e-8		1.1000e-7	0.0000
4	30.0000		1.4000e-7		1.2000e-7	0.0000
5	35.0000	1.0000e-9	2.9000e-7		1.7000e-7	0.0000
6	40.0000	2.5000e-9	4.4000e-7		2.2000e-7	0.0000
7	45.0000	4.5000e-9	6.0000e-7		3.1000e-7	0.0000
8	50.0000	9.5000e-9	8.0000e-7	1.0000e-9	3.9000e-7	0.0000
9	55.0000	2.4000e-8	1.0000e-6	1.5000e-9	4.5000e-7	0.0000

	-7- reg 20	-8- reg 25	-9- reg 27	-10- reg 29	-11- reg 31
1	2.8000e-8		2.8000e-8	1.2000e-7	0.0000
2	3.8000e-8		3.8000e-8	1.3000e-7	0.0000
3	4.8000e-8		4.8000e-8	1.4000e-7	0.0000
4	5.5000e-8		5.5000e-8	1.6000e-7	0.0000
5	6.9000e-8		6.9000e-8	1.9000e-7	0.0000
6	9.7000e-8		9.7000e-8	2.4000e-7	0.0000
7	1.5000e-7	1.5000e-9	1.5000e-7	2.8000e-7	0.0000
8	1.9000e-7	2.5000e-9	1.9000e-7	3.2000e-7	0.0000
9	2.2000e-7	2.5000e-9	2.2000e-7	3.6000e-7	0.0000

$$EFP_y = 22.6$$

EFPY = 25

$$EFPY = 27.5$$

1.8

```
.....
...
FLAW SIZE SUMMARY: PARAMETRIC VARIATION NUMBER 3 EFPY=3
.....
```

FLAW SIZE SUMMARY: PARAMETRIC VARIATION NUMBER 4 EFPY=3

FLAW SIZE SUMMARY: PARAMETRIC VARIATION NUMBER 4 EFPY=3

FLAW SIZE SUMMARY: PARAMETRIC VARIATION NUMBER 5 $EPFV=46$

FLAW SIZE SUMMARY: PARAMETRIC VARIATION NUMBER 5 EFPY=46

INITIAL LAW SIZE	NUMBER SIMULATED	% OF TOTAL	NUMBER INITIATED	% OF INITIAL INITIATIONS	NUMBER FAILED	% OF FAILURES
0.125	7885	67.6	61	4.0	61	4.0
0.200	1738	14.9	213	13.9	213	13.9
0.300	1018	8.7	434	28.2	434	28.2
0.400	479	4.1	335	21.8	335	21.8
0.500	263	2.3	228	14.8	228	14.8
0.600	149	1.3	136	8.8	136	8.8
0.700	62	0.5	59	3.8	59	3.8
0.800	50	0.4	47	3.1	47	3.1
0.900	8	0.1	8	0.5	8	0.5
1.000	4	0.0	4	0.3	4	0.3
1.100	6	0.1	6	0.4	6	0.4
1.200	2	0.0	2	0.1	2	0.1
1.300	2	0.0	2	0.1	2	0.1
1.400	0	0.0	0	0.0	0	0.0
1.499	2	0.0	2	0.1	2	0.1

Table 1.5 (cont'd)

FLAW SIZE SUMMARY: PARAMETRIC VARIATION NUMBER 6

EFPI = 45

INITIAL FLAW SIZE	NUMBER SIMULATED	% OF TOTAL	NUMBER INITIATED	% OF INITIATIONS	NUMBER FAILED	% OF FAILURES
0.125	6572	67.4	98	6.4	98	6.4
0.200	1410	14.5	206	13.4	206	13.4
0.300	847	8.7	446	29.0	446	29.0
0.400	488	5.0	376	24.5	376	24.5
0.500	209	2.1	193	12.6	193	12.6
0.600	97	1.0	92	6.0	92	6.0
0.700	64	0.7	63	4.1	63	4.1
0.800	43	0.4	41	2.7	41	2.7
0.900	7	0.1	7	0.5	7	0.5
1.000	8	0.1	8	0.5	8	0.5
1.100	3	0.0	3	0.2	3	0.2
1.200	0	0.0	0	0.0	0	0.0
1.300	4	0.0	4	0.3	4	0.3
1.400	0	0.0	0	0.0	0	0.0
1.499	0	0.0	0	0.0	0	0.0

FLAW SIZE SUMMARY: PARAMETRIC VARIATION NUMBER 7

EFPY=50

INITIAL FLAW SIZE	NUMBER SIMULATED	% OF TOTAL	NUMBER INITIATED	% OF INITIAL INITIATIONS	NUMBER FAILED	% OF FAILURES
0.125	6097	67.8	117	7.6	117	7.6
0.200	1345	15.0	259	16.9	259	16.9
0.300	794	8.8	459	29.9	459	29.9
0.400	373	4.2	332	21.6	332	21.6
0.500	201	2.2	196	12.8	196	12.8
0.600	90	1.0	88	5.7	88	5.7
0.700	42	0.5	42	2.7	42	2.7
0.800	26	0.3	26	1.7	26	1.7
0.900	4	0.0	4	0.3	4	0.3
1.000	7	0.1	7	0.5	7	0.5
1.100	5	0.1	5	0.3	5	0.3
1.200	1	0.0	1	0.1	1	0.1
1.300	1	0.0	1	0.1	1	0.1
1.400	0	0.0	0	0.0	0	0.0
1.499	0	0.0	0	0.0	0	0.0

FLAW SIZE SUMMARY: PARAMETRIC VARIATION NUMBER 8

$$EFPY = 55$$

INITIAL FLAW SIZE	NUMBER SIMULATED	% OF TOTAL	NUMBER INITIATED	% OF INITIAL INITIATIONS	NUMBER FAILED	% OF FAILURES
0.125	5838	67.9	129	8.4	129	8.4
0.200	1242	14.4	251	16.3	251	16.3
0.300	763	8.9	450	29.3	450	29.3
0.400	359	4.2	314	20.4	314	20.4
0.500	219	2.5	210	13.7	210	13.7
0.600	79	0.9	79	5.1	79	5.1
0.700	58	0.7	58	3.8	58	3.8
0.800	24	0.3	24	1.6	24	1.6
0.900	9	0.1	9	0.6	9	0.6
1.000	4	0.0	4	0.3	4	0.3
1.100	3	0.0	3	0.2	3	0.2
1.200	4	0.0	4	0.3	4	0.3
1.300	1	0.0	1	0.1	1	0.1
1.400	1	0.0	1	0.1	1	0.1
1.499	0	0.0	0	0.0	0	0.0

Table I.6. Inclusion of beneficial effect of hydro testing in calculation of $P(F/E)(HT)$

Δt EFPY(100 MW)	$t(HT)$ EFPY(100 MW)	$P(F/E)_n$	$P(F/E)_{max,n}$
1.0	22.60	2.45×10^{-6}	2.45×10^{-6}
2.0	23.60	3.00×10^{-6}	5.5×10^{-7}
3.0	25.60	4.60×10^{-6}	1.6×10^{-6}
3.0	28.60	1.09×10^{-5}	6.3×10^{-6}
3.0	31.60	2.00×10^{-5}	9.1×10^{-6}
3.0	34.60	2.85×10^{-5}	9.5×10^{-6}
3.0	37.60	3.90×10^{-5}	9.5×10^{-6}
3.0	40.60	4.82×10^{-5}	9.2×10^{-6}
3.0	43.60	5.64×10^{-5}	8.2×10^{-6}
3.0	46.60	6.40×10^{-5}	7.6×10^{-6}

**Table I.7. Inclusion of beneficial effect of modified hydro test in calculation
of $P(F/E)(E/F)$; $p("HT") = 679$ psi, $T_v("HT") = 80^\circ\text{F}$**

Δt EFPY(100 MW)	$t(HT)$ EFPY(100 MW)	$P(F/E)_n$	$P(F/E)_{max,n}$
1.0	22.60	3.31×10^{-7}	3.31×10^{-7}
2.0	23.60	4.03×10^{-7}	7.2×10^{-8}
3.0	25.60	7.01×10^{-7}	3.0×10^{-7}
3.0	28.60	3.00×10^{-6}	1.3×10^{-6}
3.0	31.60	4.20×10^{-6}	2.2×10^{-6}
3.0	34.60	6.80×10^{-6}	2.6×10^{-6}
3.0	37.60	9.40×10^{-6}	2.6×10^{-6}
3.0	40.60	1.20×10^{-5}	2.6×10^{-6}
3.0	43.60	1.42×10^{-5}	2.2×10^{-6}
3.0	46.60	1.63×10^{-5}	2.1×10^{-6}

Fig. I.1. $P(F|E)(HT)$ vs t for nozzle-corner regions (HB-1, HB-2, HB-3, HB-4); upgrade design.

Fig. I.2. $P(F/E)(HT)$ vs t for non-nozzle-corner regions; upgrade design.

Fig. I.3. Histogram of values of RT_{NDT} resulting in failures for Region 1 and hydro-test conditions.

Fig. I.4. EPRI K_{Ic} data base for commercial PWR vessel material (Ref. 38).

Fig. I.5. $P(F|E)(E/F)$ vs t for nozzle corner regions (HB-1, HB-2, HB-3, HB-4), upgrade design.

Fig. I.6. $P(F/E)(E/F)$ vs t for non-nozzle corner regions, upgrade design.

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