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Impact of Radiation Embrittlement on Integrity of Pressure Vessel Supports for Two PWR Plants

Prepared by R.D. Cheverton, W.E. Pennell, G.C. Robinson, R.K. Nanstad

Oak Ridge National Laboratory

Prepared for
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ABSTRACT

Recent data from the HFIR vessel surveillance program indicate a substantial radiation embrittlement rate effect at low irradiation temperatures ($\sim 120^{\circ}\text{F}$) for A212-B, A350-LF3, A105-II, and corresponding welds. PWR vessel supports are fabricated of similar materials and are subjected to the same low temperatures and fast neutron fluxes (10^8 to 10^9 neutrons/cm²·s, $E > 1.0$ MeV) as those in the HFIR vessel. Thus, the embrittlement rate of these structures may be greater than previously anticipated.

A study sponsored by the NRC is under way at ORNL to determine the impact of the rate effect on PWR vessel-support life expectancy. The scope includes the interpretation and application of the HFIR data, a survey of all light-water-reactor vessel support designs, and a structural and fracture-mechanics analysis of the supports for two specific PWR plants of particular interest with regard to a potential for support failure as a result of propagation of flaws. Calculations performed thus far indicate best-estimate critical flaw sizes, corresponding to 32 EFPY, of ~ 0.2 in. for one plant and ~ 0.4 in. for the other. These flaw sizes are small enough to be of concern. However, it appears that low-cycle fatigue is not a viable mechanism for creation of flaws of this size, and thus, presumably, such flaws would have to exist at the time of fabrication.

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FOREWORD

The work reported here was performed at Oak Ridge National Laboratory under the Heavy-Section Steel Technology (HSST) Program, W. R. Corwin, Program Manager. The program is sponsored by the Office of Nuclear Regulatory Research of the U.S. Nuclear Regulatory Commission (NRC). The technical monitor for the NRC is M. E. Mayfield.

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LIST OF ACRONYMS

AE	architect engineer
ASME	American Society of Mechanical Engineers
ASTM	American Society for Testing and Materials
B&W	Babcock & Wilcox Company
BWR	boiling-water reactor
CE	Combustion Engineering
CVN	Charpy V-notch
DL	dead load
dpa	displacements per atom
DW	dead weight
E	operating base earthquake
E'	no-loss-of-function earthquake
EFPY	effective full-power years
EPRI	Electric Power Research Institute
EGCR	Experimental Gas-Cooled Reactor
FSAR	Final Safety Analysis Report
GE	General Electric
HAZ	heat-affected zone
HFIR	High Flux Isotope Reactor
LBLOCA	large-break loss-of-coolant accident
LEFM	linear elastic fracture mechanics
LOCA	loss-of-coolant accident
LWR	light-water reactor
MTR	Materials Testing Reactor
NDTT	nil ductility transition temperature
Δ NDTT	increase in nil ductility transition temperature
NOAC	Nuclear Operations Analysis Center
NSSS	nuclear steam supply system
NRC	Nuclear Regulatory Commission
OBE	operating basis earthquake
ORNL	Oak Ridge National Laboratory
ORR	Oak Ridge Research Reactor
PGE	Portland General Electric
PSAR	Preliminary Safety Analysis Report
PWR	pressurized-water reactor
SBLOCA	small-break loss-of-coolant accident
SEN	single-edge notch
SSE	safe shutdown earthquake
<u>W</u>	Westinghouse Electric Corporation

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IMPACT OF RADIATION EMBRITTLEMENT ON INTEGRITY OF
PRESSURE-VESSEL SUPPORTS FOR TWO PWR PLANTS

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1. INTRODUCTION AND SUMMARY

Structural supports for most pressurized-water-reactor (PWR) pressure vessels are located in the cavity between the vessel and the biological shield (Fig. 1.1). Within the cavity the fast neutron flux (ϕ) for energies $E > 1.0$ MeV is $\leq 2 \times 10^9$ neutrons/cm²·s, and temperatures are $< 150^\circ\text{F}$. The corresponding calculated increase in the nil ductility

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Fig. 1.1. PWR vessel support located in cavity between vessel and biological shield.

transition temperature (NDTT) by 32 effective full-power years (EFPY), based on the radiation embrittlement data available from materials testing reactors (MTRs) before 1987,^{1,2} is quite small, if the difference in the MTR and the PWR cavity fast neutron energy spectra is neglected.

Early in 1978 it became apparent that the fast neutron spectrum above 0.1 MeV was much softer in the PWR cavity than in the MTRs (the result of inelastic scattering in the PWR vessel wall), and thus correlating the MTR embrittlement data with fast neutron fluence (Φ) for $E > 1.0$ MeV resulted in an underestimation of the increase in the nil ductility transition temperature (Δ NDTT) for supports in the cavity. As a part of the more recent study discussed herein, the MTR data were correlated with displacements per atom (dpa) for $E > 0.1$ MeV, and Δ NDTT values of $\leq 70^\circ\text{F}$ were calculated for 32 EFPY for supports located in the cavity at midheight of the core.

Several studies pertaining to radiation damage of PWR vessel supports were conducted between 1978 and 1987,³⁻⁶ and during this period, presumably there was no reason to believe that low-temperature ($< 200^\circ\text{F}$) MTR embrittlement data, correlated with dpa ($E > 0.1$ MeV), were not appropriate for evaluating embrittlement of PWR vessel supports. However, late in 1986, data from the High Flux Isotope Reactor (HFIR)⁷ vessel surveillance program^{8,9} indicated that the embrittlement rates of the several vessel materials (A212-B, A350-LF3, and A105-II) were substantially greater than anticipated on the basis of MTR data.⁹ Further evaluation of the HFIR data suggested that a fluence-rate effect was responsible for the apparent discrepancy because the fast neutron flux in the MTR that provided the design data¹ was $\sim 10^5$ times that in the HFIR vessel, while the small differences in the fast neutron energy spectra were accounted for by correlating the data with dpa ($E > 0.1$ MeV), and the irradiation temperatures were nearly the same (120 to 200°F). As a result of this new information, the Nuclear Regulatory Commission (NRC) requested that the Oak Ridge National Laboratory (ORNL) evaluate the impact of the apparent embrittlement rate effect on the integrity of light-water-reactor (LWR) vessel supports.

The purpose of this first report is to provide a preliminary indication of whether the integrity of reactor vessel supports is likely to be challenged by radiation-induced embrittlement before 32 EFPY. Because of the diversity in support designs, specific-plant evaluations were undertaken, and because of the urgency associated with the preliminary report, only readily available data for these plants were used. This and other factors have resulted in some inconsistencies in the evaluations of the plants considered. Even so, the report satisfies its intended purpose.

The scope of the ORNL evaluation included

1. correlation of the HFIR data for application to the evaluation of LWR vessel supports,
2. a survey and cursory evaluation of all U.S. LWR vessel support designs,
3. selection of two plants in accordance with established criteria for specific-plant evaluation, and
4. a specific-plant evaluation of both plants to determine critical flaw sizes for their vessel supports.

The two plants selected for specific-plant evaluation were Trojan (Portland General Electric) and Turkey Point Unit 3 (Florida Power and Light), both of which are PWR plants with a vessel support similar to that shown in Fig. 1.1. Westinghouse was the nuclear-steam-supply-system (NSSS) designer, and Bechtel was the architect-engineer for both plants. The utilities and their contractors have been very cooperative in providing, where possible, design data required for the ORNL study.

Over the course of several months, two sets of radiation-damage trend curves (Δ NDTT vs dpa), based on the HFIR vessel surveillance data, were developed, and 32-EFPY Δ NDTT values were calculated for "typical" General Electric (GE), Babcock and Wilcox (B&W), Westinghouse (W), and Combustion Engineering (CE) plants, assuming that a critical portion of a support existed in the cavity at midheight of the core. The results, presented in Table 1.1, indicate much larger shifts in NDTT based on the HFIR data than on the MTR data.

Table 1.1. Vessel support Δ NDTT values corresponding to 32 EFPY and midheight of core ("typical" LWR plants)

NSSS designer (type reactor)	ϕ ($E > 1$ MeV) (neutrons/cm ² ·s)	dpa rate ($E > 0.1$ MeV) (s ⁻¹)	dpa	Δ NDTT (°F)		
				MTR data	HFIR data	
					A ^a	B ^a
GE (BWR)	2.9×10^7	5.8×10^{-14}	5.8×10^{-5}	0	b	b
B&W (PWR)	2.0×10^8	6.1×10^{-13}	6.1×10^{-4}	20	180	130
W (PWR)	5.9×10^8	3.9×10^{-12}	3.9×10^{-3}	50	240	220
CE (PWR)	1.8×10^9	4.5×10^{-12}	4.5×10^{-3}	70	250	220

^a Δ NDTT vs dpa ($E > 0.1$ MeV) correlations A and B.

^b Δ NDTT not estimated for dpa $< 10^{-4}$.

Many of the vessel supports are not located at midheight of the core and thus experience smaller shifts in NDTT than those indicated in Table 1.1. For instance, all but one of the GE boiling-water-reactor (BWR) vessels and all but one of the B&W PWR vessels are supported by a skirt at the bottom of the vessel where the fluxes are much less. However, ~25% of the PWR vessel supports are exposed to the peak flux, and many others are exposed to fluxes within a factor of one-half of the peak.

The concern over radiation embrittlement is that it increases the potential for propagation of flaws that might exist in the support structures. In this study the potential for propagation of flaws was evaluated using linear-elastic fracture mechanics, which requires knowledge of the stresses in the structure and the fracture toughness of the

material. The output of the fracture-mechanics analysis for this study is the critical flaw size, that is, the size of the smallest flaw that will propagate under a given set of assumed conditions and will result in failure of the support. If the critical flaw size is small enough that the critical flaw is likely to exist, then the frequency of failure is equal to the frequency of application of the assumed load. A determination of the probability of the existence of flaws was not, however, included in the scope of this study.

Loading conditions were provided by the utilities and included large- and small-break loss-of-coolant accidents (LBLOCA and SBLOCA), seismic loading, thermal loading, and dead-weight loading. Dynamic loading conditions were available for Trojan but were not available for Turkey Point.

Several uncertainties exist in the analysis, and those judged to be the most significant are related to the radiation-damage trend curve deduced from the HFIR surveillance data, the fracture toughness of the material, including the initial NDTT, and the normal operating temperature of the structure at the critical location (T). These uncertainties result in a wide variation in calculated critical flaw size. The critical flaw size is, of course, also dependent on load and the location and type of flaw. Critical flaw sizes (depth/surface length) were calculated for a reasonable range of conditions, and 32-EFPY "best-estimate" values for the most sensitive location and type of flaw and for the most severe credible loading conditions are 0.4/2.5 in. for Trojan and 0.3/0.6 in. for Turkey Point (Table 1.2). Corresponding values for late 1988 are 0.9/16 and 0.3/0.6 in., respectively.

Table 1.2. Summary of best-estimate minimum critical flaw sizes and values of T - NDTT for Trojan and Turkey Point vessel supports (cantilever beam)

Plant	EFPY	T - NDTT (°F)		Critical flaw size (depth × surface length) (in.)		
		a	b	LBLOCA	SBLOCA	Seismic (SSE) ^c
Trojan	7.5 ^d	-12	-12		0.9 × 16.0	1.1 × 16.0
	32	-13	-118		0.4 × 2.5	1.2 × 2.5
Turkey Point	11.8 ^d	28	10	0.3 × 0.6		1.0 × 2.0
	32	-25	-25	0.3 × 0.6		0.9 × 1.8

^aAt location of minimum-depth critical flaw.

^bAt inner surface of biological shield (cavity interface).

^cSafe shutdown earthquake.

^dLate 1988.

For both plants the flaw location resulting in the smallest critical flaw depth was on the upper flange of the horizontal cantilever beam at a point within the concrete biological shield (Fig. 1.1). The best-estimate values of $T - NDTT$ at these locations are -13 and -25°F for 32 EFY and -12 and 28°F for late 1988 for Trojan and Turkey Point, respectively.

The propagation of flaws by low-cycle fatigue was calculated to be negligible. Thus, if corrosion is not a viable means of growing flaws to critical size or larger, flaws of critical size or larger would have to be present at the time of fabrication. As already mentioned, this report does not address the question regarding the probability of existence of flaws.

Details regarding the HFIR vessel surveillance data, development of the corresponding embrittlement trend curves, a survey and cursory evaluation of the LWR support designs, selection of two LWR plants for specific-plant evaluation of the supports, and the specific-plant analyses are included in the following sections of this report.

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*Available for purchase from National Technical Information Service, Springfield, Virginia 22161.

2. HFIR VESSEL SURVEILLANCE DATA

The High Flux Isotope Reactor¹ is a high-performance, light-water-cooled, low-temperature (120 to 160°F) research reactor that began operation in 1965 at ORNL. Its stainless-steel-clad, carbon-steel pressure vessel (Fig. 2.1) was designed for 20 EFPY, and a surveillance program was maintained to monitor the actual radiation-induced embrittlement.² Late in 1986, a reevaluation of the integrity of the vessel was commenced in an effort to extend the permissible life.³ The surveillance data, which had not been carefully examined since 1974, indicated that the embrittlement rate was significantly greater than had

Fig. 2.1. Cross section of HFIR vessel and core at midheight of core, indicating locations of vessel surveillance specimens (Keys 1-7).

been anticipated on the basis of data obtained in the early 1960s from MTRs.⁴ The neutron energy spectra and the irradiation temperatures for the HFIR surveillance specimens and for specimens in the MTRs were believed to be essentially the same, and the materials were very similar; however, the fast neutron flux in the MTRs was about 10^5 times that in the HFIR specimens. Thus, it appeared that the lower flux in HFIR was responsible for the relatively large amount of embrittlement per neutron; that is, there appeared to be a negative fluence-rate effect.

The portions of the HFIR vessel that are subjected to the highest fast-neutron fluxes are close to the beam tubes (Fig. 2.1) because the beam tubes displace beryllium (reflector) and water that otherwise constitute shielding for the vessel wall. Thus, both shell material (A212-B) and beam-tube nozzle materials (A105-II and A350-LF3) were included in the HFIR vessel materials surveillance program, and the surveillance capsules, each containing three Charpy V-notch (CVN) specimens and a flux monitor, were located close to the beam tubes (keys 1-7, Fig. 2.1).

Surveillance specimens of A212-B were removed for testing after 15.0 and 17.5 EFPY, and A105-II and A350-LF3 specimens were removed after 2.3, 6.5, 15.0, and 17.5 EFPY. The corresponding Δ NDTT data are presented in Table 2.1;⁴ in Figs. 2.2 and 2.3, plots of Δ NDTT vs fast neutron fluence (ϕ) ($E > 1.0$ MeV) are compared with the MTR data⁵ available when the vessel was designed. If it is assumed that spectrum and chemistry effects are not responsible for the incongruity of the several sets of data, the comparison indicates a fluence-rate effect.

To evaluate the effects of possible differences in chemistry and fast spectrum, HFIR archive A212-B material was recently irradiated in the Oak Ridge Research Reactor (ORR), a typical MTR, and the HFIR and ORR A212-B data were plotted as a function of dpa for $E > 0.1$ MeV (Fig. 2.4)* as well as a function of ϕ for $E > 1.0$ MeV (Figs. 2.2 and 2.3). (The ORR data and values of dpa corresponding to both the HFIR and ORR data are included in Table 2.1.) Figure 2.2 shows the A212-B archive material (irradiated in the ORR) to be consistent with the MTR data, implying that the chemistry of the HFIR A212-B material is not significantly different than that corresponding to the MTR data (assuming that a difference in spectrum does not compensate for a difference in chemistry). Furthermore, surveillance data from Army reactors⁶ (Fig. 2.5) indicate no significant difference in sensitivity between A212-B and A350-LF3, at least for the specific exposure conditions. These latter data also indicate that for the relatively high fluxes in the Army reactors, the increase in NDTT for A212-B at a fluence of 1 to 2×10^{17} neutrons/cm² is much less than that obtained from HFIR, indicating that the HFIR data are not simply a part of the tail of the MTR data.

Figure 2.4 shows that when the HFIR and MTR data are plotted as a function of dpa ($E > 0.1$ MeV), there is still evidence of a significant rate effect. Thus, the small differences in the HFIR and MTR fast spectra are not responsible for the incongruity in Figs. 2.2 and 2.3.

*To obtain the MTR curve, it was assumed that the calculated spectrum for the ORR was appropriate for the "MTR" data.

Table 2.1. Summary of irradiation effect for selected HFIR pressure vessel materials

Material	Unirradiated Δ NDTT [°C (°F)]	EFPY	Δ NDTT [°C (°F)]	ϕ (E > 1 MeV) (neutrons/cm ² ·s)	dpa/s	ϕ (E > 1 MeV) (neutrons/cm ²)	dpa
<i>ORR irradiations</i>							
A212-B (LT)	-21 (-5)		56 (101)	9.59×10^{12}	1.34×10^{-8}	2.43×10^{18}	3.39×10^{-3}
A212-B (TL)	-12 (10)		47 (85)	9.59×10^{12}	1.34×10^{-8}	2.43×10^{18}	3.39×10^{-3}
A212-B (TS)	-21 (-5)		56 (101)	9.59×10^{12}	1.34×10^{-8}	2.43×10^{18}	3.39×10^{-3}
A212-B (EGCR) ^a	0 (32)		10 (18)	1.05×10^{13}	1.46×10^{-8}	1.54×10^{17}	2.14×10^{-4}
A212-B (EGCR) ^a	0 (32)		103 (185)	2×10^{12}	4.8×10^{-9}	9.8×10^{18}	2.4×10^{-2}
<i>HFIR surveillance</i>							
A212-B (LT)	-21 (-5)	15.01	11 (20)	4×10^7		1.89×10^{16}	
		15.01	29 (52)	2.43×10^8	3.66×10^{-13}	1.15×10^{17}	1.73×10^{-4}
		17.53	42 (75)	2.43×10^8	3.66×10^{-13}	1.34×10^{17}	2.02×10^{-4}
A105-II	-62 (-80)	2.34	10 (18)	4.66×10^8	6.92×10^{-13}	3.44×10^{16}	5.12×10^{-5}
		6.45	17 (30)	4.89×10^8	7.26×10^{-13}	9.9×10^{16}	1.48×10^{-4}
		15.01	33 (60)	4.89×10^8	7.26×10^{-13}	2.31×10^{17}	3.44×10^{-4}
		15.01	33 (60)	3.35×10^8	4.89×10^{-13}	1.85×10^{17}	2.70×10^{-4}
		17.53	35 (63)	7.27×10^8	1.08×10^{-12}	4.01×10^{17}	5.97×10^{-4}
A350-LF3 (key 2)	-79 (-110)	2.34	14 (26)	1.1×10^9	1.58×10^{-12}	8.20×10^{16}	1.16×10^{-4}
		6.45	29 (52)	1.1×10^9	1.58×10^{-12}	2.26×10^{17}	3.21×10^{-4}
		15.01	56 (100)	1.1×10^9	1.58×10^{-12}	5.26×10^{17}	7.47×10^{-4}
		17.53	66 (118)	1.1×10^9	1.58×10^{-12}	6.14×10^{17}	8.73×10^{-4}
A350-LF3 (key 3)	-62 (-80)	2.34	19 (34)	1.29×10^9	1.85×10^{-12}	9.55×10^{16}	1.36×10^{-4}
		6.45	33 (60)	1.40×10^9	2.01×10^{-12}	2.84×10^{17}	4.09×10^{-4}
		15.01	54 (97)	1.03×10^9	1.48×10^{-12}	4.88×10^{17}	7.01×10^{-4}
		17.53	64 (115)	1.29×10^9	1.85×10^{-12}	7.12×10^{17}	1.02×10^{-3}

^aMaterial from previous ORNL study on Experimental Gas-Cooled Reactor.

Fig. 2.2. Increase in NDTT with fluence (E > 1.0 MeV) for A212-B irradiated in HFIR (vessel surveillance positions) and ORR and for several similar materials irradiated in MTR.⁵

Fig. 2.3. Increase in NDTT with fluence (E > 1 MeV) for irradiations in HFIR (vessel surveillance positions) and MTR.⁵

Fig. 2.4. Increase in NDTT with dpa for irradiations in HFIR (vessel surveillance positions), ORR, and MTR.⁵

If an embrittlement rate effect exists, the total reaction rate associated with embrittlement should probably be accounted for when applying the HFIR data to a situation involving a different fast spectrum. This can be accomplished by correlating NDTT with dpa rate rather than ϕ , integrating the dpa rate over all energies that contribute significantly to embrittlement.

In the previous discussions, the only spectral variations considered were those above 0.1 MeV. However, Nanstad et al.⁷ have suggested that lower energy neutrons may be making a significant contribution and that there might be significant differences in the low-energy spectra of HFIR and the MTRs. Multigroup transport calculations performed at ORNL⁸ indicate that the HFIR spectrum above 1.0 MeV is somewhat harder than that in the ORR; the epithermal flux is a smaller fraction of the total nonthermal flux; and ϕ_{th}/ϕ_f , the ratio of the thermal to fast ($E > 1.0$ MeV), is greater in HFIR (~50) than in ORR (~8). Thus, the trend in Figs. 2.2 and 2.3 is not the result of differences in the epithermal fluxes, but the comparison of ϕ_{th}/ϕ_f suggests a possible thermal-flux effect. In addition, Alborman et al.⁹ irradiated A537 at 140°F in a spectrum with $\phi_{th}/\phi_f \sim 1000$ and found that >70% of the embrittlement was caused by thermal neutrons. Thus, perhaps thermal-neutron embrittlement is at least in part responsible for what was otherwise believed to be a rate effect.¹⁰ At this time, however, there are not enough data to support this contention.

Fig. 2.5. Effects of neutron radiation on Charpy-V transition temperature behavior of Army RPV steels. (Numbers adjacent to data points indicate exposure temperatures.) Source: Ref. 6.

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3. APPLICATION OF HFIR DATA TO VESSEL SUPPORT EVALUATION

Soon after the HFIR surveillance data were evaluated in late 1986, it became apparent that the indicated embrittlement rate effect might apply to the supports of some LWR vessels because fast neutron fluxes, irradiation temperatures, and materials were thought to be similar. Temperatures of the supports range from $\sim 500^{\circ}\text{F}$, at the point where they contact the vessel, to $<150^{\circ}\text{F}$ at a point where they contact the biological shield (150°F is the normal maximum permissible operating temperature of the concrete biological shield). The temperature of the HFIR vessel and surveillance specimens is $\sim 120^{\circ}\text{F}$. Thus, presumably a portion of the support operates at a temperature close to that of the HFIR vessel.

Multigroup neutron transport calculations were performed recently for the vessel wall and the cavity of one BWR and three PWRs, and the results are provided in Tables 3.1–3.8.^{1,2} Table 3.9 summarizes the fast fluxes ($E > 1.0$ MeV) [as well as dpa rate ($E > 0.1$ MeV)] for the LWR cavities and the HFIR surveillance specimens (also, see Table 2.1). It is apparent that ϕ ($E > 1.0$ MeV) for the PWR cavities is similar to that for the HFIR surveillance specimens (10^8 to 10^9 neutrons/cm²·s), while that for the BWR is much less.

Table 3.10 summarizes LWR fast-flux data for $E > 1.0$ MeV (group A) and $0.1 \leq E \leq 1.0$ MeV (group B). These data indicate that the ratio of group A to group B fluxes is much less in the cavity than it is at the inner surface of the vessel wall (the result of inelastic scattering in the vessel wall). Thus, the fast flux ($E > 0.1$ MeV) in the LWR cavity is much softer than that at the location of the HFIR surveillance specimens. As suggested in Chap. 2, to account for this difference in energy spectrum when applying the HFIR data to the evaluation of the supports in the cavity, the ΔNDTT data can be correlated with dpa rate and dpa for $E > 0.1$ MeV instead of ϕ and ϕ for $E > 1.0$ MeV, the assumption being made that most of the neutrons contributing to embrittlement have energies >0.1 MeV. A comparison of dpa rate ($E > 0.1$ MeV) for HFIR and the LWR cavities (Table 3.9) indicates that the maximum cavity dpa rate ($5.0 \times 10^{-12} \text{ s}^{-1}$) is about twice the maximum HFIR dpa rate (2.0×10^{-12}), while the maximum fast flux values ($E > 1.0$ MeV) are about the same. This indicates that some extrapolation of the HFIR data is necessary.

Application of the HFIR data to the LWR vessel supports requires extrapolation with regard to both dpa rate and dpa. Thus, correlations between ΔNDTT , dpa rate, and dpa are required. Two different correlations between ΔNDTT and dpa are proposed and are shown in Figs. 3.1 and 3.2. Figure 3.1 was created by drawing separate straight-line curves through the maximum ΔNDTT data points, for the A212-B and A350-LF3 HFIR surveillance materials, parallel to the materials testing reactor (MTR) curve, which Hawthorne constructed as a straight line on semilog paper.³

The other correlation (Fig. 3.2) was obtained by first plotting the HFIR A350-LF3 data on log-log paper and constructing a best-fit straight-line curve. Next, the ORR A212-B data obtained in connection with the recent HFIR vessel study⁴ and a much earlier study of the vessel for the Experimental Gas-Cooled Reactor (EGCR)⁵ (Table 2.1) were

Table 3.1. Calculated (DOT, 3-D) neutron energy spectra for B&W (ANO-1) reactor

Group	E (upper) (MeV)	Flux (neutrons/cm ² ·s)		
		0T ^a	T/4	Cavity
1	1.733+01	2.699+07	1.258+07	5.175+05
2	1.221+01	6.543+07	2.940+07	1.061+06
3	1.000+01	2.743+08	1.205+08	3.694+06
4	7.408+00	7.879+08	3.242+08	7.908+06
5	4.966+00	1.133+09	4.882+08	1.348+07
6	3.012+00	7.621+08	3.822+08	1.244+07
7	2.466+00	3.448+08	1.794+08	5.967+06
8	2.307+00	1.376+09	9.010+08	3.893+07
9	1.653+00	2.000+09	1.688+09	1.176+08
10	1.003+00	1.108+09	9.956+08	1.036+08
11	7.427-01	1.863+09	2.314+09	3.413+08
12	4.979-01	2.052+09	3.036+09	5.289+08
13	2.972-01	1.171+09	1.214+09	2.908+08
14	1.832-01	1.235+09	1.643+09	3.327+08
15	1.111-01	8.378+08	1.303+09	2.556+08
16	6.738-02	3.851+08	6.444+08	1.953+08
17	3.183-02	1.820+08	7.730+07	2.832+07
18	2.606-02	3.043+08	4.703+08	1.094+08
19	2.418-02	6.040+08	6.469+08	2.004+08
20	1.503-02	6.938+08	3.110+08	1.652+08
21	7.102-03	2.697+09	1.528+09	4.918+08
22	4.540-04	1.489+09	8.032+08	2.364+08
23	1.013-04	3.968+09	1.807+09	4.823+08
24	1.855-06	1.272+09	3.777+08	1.309+08
25	4.140-07	9.277+09	2.232+08	5.184+08
26	1.000-11			
Total		3.641+10	2.152+10	4.613+09

^aInner surface of vessel.

Table 3.2. 4-Group fluxes and total dpa rate for B&W (ANO-1) reactor

Group	Energy range	Flux ^a		
		0T ^b	1/4T	Cavity
1	1.0 MeV-17 MeV	0.186	0.192	0.044
2	0.111 MeV-1.0 MeV	0.204	0.428	0.346
3	0.4 eV-0.111 MeV	0.355	0.370	0.498
4	10 ⁻⁵ -0.4 eV	0.255	0.010	0.112
dpa/s (E > 10 ⁻⁵ eV)		1.041E-11	7.232E-12	6.829E-13

^aNormalized to unity.

^bInner surface of vessel.

Table 3.3. Calculated (DOT, 3-D) neutron energy spectra for CE (ANO-2) reactor

Group	E (upper) (MeV)	Flux (neutrons/cm ² ·s)		
		OT ^a	T/4	Cavity
1	1.733+01	7.373+07	3.189+07	1.622+06
2	1.221+01	2.330+08	9.728+07	4.384+06
3	1.000+01	1.265+09	5.148+08	1.990+07
4	7.408+00	4.565+09	1.724+09	5.359+07
5	4.966+00	7.486+09	2.910+09	9.773+07
6	3.012+00	5.313+09	2.383+09	9.108+07
7	2.466+00	2.438+09	1.129+09	4.406+08
8	2.307+00	9.928+09	5.725+09	2.754+08
9	1.653+00	1.492+10	1.104+10	8.080+08
10	1.003+00	8.237+09	6.600+09	7.108+08
11	7.427-01	1.460+10	1.602+10	2.258+09
12	4.979-01	1.567+10	2.041+10	3.328+09
13	2.972-01	8.603+09	8.400+09	1.932+09
14	1.832-01	9.282+09	1.138+10	2.164+09
15	1.111-01	6.773+09	8.073+09	1.585+09
16	6.738-02	6.212+09	4.538+09	1.320+09
17	3.183-02	9.359+08	4.206+08	2.006+08
18	2.606-02	2.570+09	3.218+09	7.005+08
19	2.418-02	4.319+09	4.496+09	1.320+09
20	1.503-02	4.580+09	2.194+09	1.119+09
21	7.102-03	1.829+10	9.711+09	3.355+09
22	4.540-04	9.975+09	4.705+09	1.626+09
23	1.013-4	2.731+10	1.172+10	3.458+09
24	1.855-06	8.374+09	2.297+09	9.823+08
25	4.140-07	5.962+10	1.753+09	4.648+09
26	1.000-11			
27				
Total		2.516+11	1.415+11	3.254+10

^aInner surface of vessel.

Table 3.4. 4-Group fluxes and total dpa rate for CE (ANO-2) reactor

Group	Energy range	Flux ^a		
		OT ^b	1/4T	Cavity
1	1.0 MeV–17 MeV	0.184	0.181	0.055
2	0.111 MeV–1.0 MeV	0.224	0.444	0.319
3	0.4 eV–0.111 MeV	0.355	0.363	0.483
4	10 ⁻⁵ eV–0.4 eV	0.237	0.012	0.143
dpa/s (E > 10 ⁻⁵ eV)		7.041E-11	4.549E-11	4.975E-12

^aNormalized to unity.

^bInner surface of vessel.

Table 3.5. Calculated (DOT, 3-D) neutron energy spectra for W reactor McQuire Unit 1, Cycle 1

Group	E (upper) (MeV)	Flux (neutrons/cm ² ·s)		
		OT ^a	T/4	Cavity (R = 323.83 cm)
1	1.733+01	5.062+07	2.105+07	3.020+05
2	1.221+01	1.567+08	6.249+07	7.764+05
3	1.000+01	8.316+08	3.236+08	3.293+06
4	7.408+00	2.893+09	1.048+09	8.135+06
5	4.966+00	4.424+09	1.662+09	2.014+07
6	3.012+00	3.001+09	1.331+09	2.193+07
7	2.466+00	1.360+09	6.255+08	1.135+07
8	2.307+00	5.295+09	3.126+09	9.912+07
9	1.653+00	7.603+09	5.901+09	4.448+08
10	1.003+00	4.142+09	3.448+09	4.510+08
11	7.427-01	6.947+09	8.317+09	2.546+09
12	4.979-01	7.404+09	1.087+10	4.565+09
13	2.972-01	4.298+09	4.316+09	2.449+09
14	1.832-01	4.468+09	6.069+09	3.419+09
15	1.111-01	3.380+09	4.375+09	2.604+09
16	6.738-02	3.362+09	2.360+09	2.024+09
17	3.183-02	6.466+08	2.369+08	3.793+08
18	2.606-02	1.153+09	1.697+09	1.490+09
19	2.418-02	2.209+09	2.410+09	2.812+09
20	1.503-02	2.657+09	1.170+09	1.946+09
21	7.102-03	1.016+10	5.142+09	5.942+09
22	4.540-04	5.600+19	2.547+09	2.867+09
23	1.013-04	1.473+10	5.865+09	5.993+09
24	1.855-06	4.670+09	1.151+09	1.680+09
25	4.140-07	3.382+10	7.968+08	7.164+09
26	1.000-11			
27				
Total		1.353+11	7.487+10	4.894+10

^aInner surface of vessel.

Table 3.6. 4-Group fluxes and total dpa rate for W reactor

Group	Energy range	Flux ^a		
		OT ^b	1/4T	Cavity
1	1.0 MeV-17 MeV	0.184	0.188	0.012
2	0.111 MeV-1.0 MeV	0.202	0.441	0.274
3	0.4 eV-0.411 MeV	0.359	0.360	0.567
4	10 ⁻⁵ eV-0.4 eV	0.250	0.011	0.146
dpa/s (E > 10 ⁻⁵ eV)		3.874E-11	2.484E-11	4.712E-12

^aNormalized to unity.

^bInner surface of vessel.

Table 3.7. Calculated (DOT, 3-D) neutron energy spectra for GE reactor

Group	E (upper) (MeV)	Flux (neutrons/cm ² ·s)		
		OT ^a	T/4	Cavity
1	1.733E+01	2.78532E+05	1.19476E+05	1.26309E+04
2	1.419E+01	1.17743E+06	5.10065E+05	5.31195E+04
3	1.221E+01	4.20585E+06	1.73173E+06	1.64842E+05
4	1.000E+01	7.86872E+06	3.22077E+06	2.92429E+05
5	8.607E+00	1.27925E+07	5.10243E+06	4.31247E+05
6	7.408E+00	2.97338E+07	1.15308E+07	8.93362E+05
7	6.065E+00	3.82946E+07	1.42439E+07	1.06782E+06
8	4.966E+00	5.75978E+07	2.15559E+07	1.67350E+06
9	3.679E+00	3.59718E+07	1.51213E+07	1.29333E+06
10	3.012E+00	2.41009E+07	1.12497E+07	9.89848E+05
11	2.725E+00	2.59448E+07	1.28555E+07	1.19784E+06
12	2.466E+00	1.25811E+07	6.32145E+06	6.21796E+05
13	2.365E+00	3.22105E+06	1.78435E+06	1.76455E+05
14	2.346E+00	1.50974E+07	8.68747E+06	8.79155E+05
15	2.231E+00	3.58901E+07	2.23694E+07	2.14032E+06
16	1.920E+00	3.56560E+07	2.62479E+07	2.89304E+06
17	1.653E+00	4.59384E+07	3.65276E+07	4.35308E+06
18	1.353E+00	6.38427E+07	6.42605E+07	9.28453E+06
19	1.003E+00	4.05210E+07	4.46711E+07	7.60235E+06
20	8.208E+01	2.11290E+07	1.92272E+07	4.04310E+06
21	7.427E+01	4.78865E+07	6.94558E+07	1.34120E+07
22	6.081E+01	4.06699E+07	5.56024E+07	1.30706E+07
23	4.979E+01	4.48645E+07	6.27020E+07	1.33038E+07
24	3.688E+01	4.04880E+07	6.98931E+07	1.48399E+07
25	2.972E+01	6.22632E+07	7.57791E+07	2.24843E+07
26	1.832E+01	5.40647E+07	7.47653E+07	2.27497E+07
27	1.111E-01	4.23644E+07	4.61578E+07	1.65013E+07
28	6.738E-02	3.61008E+07	3.55079E+07	1.25341E+07
29	4.087E-02	1.56710E+07	9.46462E+06	4.06071E+06
30	3.183E-02	1.18315E+07	2.92956E+06	4.08343E+06
31	2.606E-02	7.64521E+06	1.84057E+07	5.40984E+06
32	2.418E-02	7.29383E+06	1.11433E+07	3.93861E+06
33	2.188E-02	2.23029E+07	1.78285E+07	8.46670E+06
34	1.503E-02	4.12070E+07	2.00703E+07	1.19762E+07
35	7.102E-03	4.04597E+07	2.52165E+07	1.15085E+07
36	3.355E-03	3.90943E+07	1.99575E+07	1.03076E+07
37	1.585E-03	6.33659E+07	3.07840E+07	1.57353E+07
38	4.540E-04	3.75323E+07	1.35174E+07	8.53409E+06
39	2.144E-04	3.76390E+07	1.74130E+07	8.29094E+06
40	1.013E-04	5.01136E+07	2.50163E+07	1.05576E+07
41	3.727E-05	6.23387E+07	3.06902E+07	1.23638E+07
42	1.068E-05	3.71304E+07	1.64081E+07	6.92802E+06
43	5.043E-06	4.87669E+07	1.80571E+07	8.48279E+06
44	1.855E-06	3.58256E+07	9.95936E+06	5.74201E+06
45	8.764E-07	3.49526E+07	6.91034E+06	5.16106E+06
46	4.140E-07	1.19921E+08	6.44878E+06	1.10811E+07
47	1.000E-07	1.25993E+09	9.40594E+06	3.51009E+07
48	1.000E-11			
Total	2.8536E+09	1.1268E+09	3.5669E+08	

^aInner surface of vessel.

Table 3.8. 4-Group fluxes and total dpa rate for GE reactor

Group	Energy range	Flux ^a				
		Core boundary	Mid-downcomer	OT ^b	1/4T	Cavity
1	1.0 MeV–17 MeV	0.096	0.149	0.158	0.234	0.080
2	0.111 MeV–1.0 MeV	0.314	0.099	0.123	0.419	0.313
3	0.4 eV–0.111 MeV	0.460	0.237	0.235	0.333	0.478
4	10 ⁻⁵ eV–0.4 eV	0.130	0.515	0.484	0.014	0.129
dpa/s (E > 10 ⁻⁵ eV)				6.909E-13	4.303E-13	6.335E-14

^aNormalized to unity.^bInner surface of vessel.

Table 3.9. Summary of fast-neutron fluxes (E > 0.1 MeV) for LWR cavities and HFIR surveillance specimens

Reactor	ϕ (E > 1.0 MeV) (neutrons/cm ² ·s)	dpa/s (E > 0.1 MeV)
HFIR	2.4×10^8 – 1.4×10^9	3.7×10^{-13} – 2.0×10^{-12}
GE (BWR)	2.8×10^7	6.3×10^{-14}
B&W (PWR)	2.1×10^8	6.8×10^{-13}
W (PWR)	6.1×10^8	4.7×10^{-12}
CE (PWR)	1.8×10^9	5.0×10^{-12}

Table 3.10. Summary of calculated fluxes for "typical" BWR and PWR vessels and cavities

Reactor	Neutron flux (neutrons/cm ² ·s)					
	Vessel inner surface			Cavity		
	A ^a	B ^a	A/B	A	B	A/B
GE (BWR)	6.3×10^8	3.5×10^8	1.8	2.8×10^7	1.1×10^8	0.3
B&W (PWR)	5.8×10^9	7.4×10^9	0.8	2.1×10^8	1.6×10^9	0.1
W (PWR)	2.6×10^{10}	2.7×10^{10}	1.0	6.1×10^8	1.3×10^{10}	0.5
CE (PWR)	4.6×10^{10}	5.6×10^{10}	0.8	1.8×10^9	1.0×10^{10}	0.2

^aA: E > 1.0 MeV; B: 0.1 ≤ E ≤ 1.0 MeV.

Fig. 3.1. Method for extrapolating HFIR vessel surveillance ΔNDTT vs dpa ($E > 0.1 \text{ MeV}$) data, assuming $\Delta \text{NDTT} = 145 \ln \text{dpa}/d^\circ\text{F}$ for $\Delta \text{NDTT} > 50^\circ\text{F}$.

Fig. 3.2. Method for extrapolating HFIR vessel surveillance ΔNDTT vs dpa ($E > 0.1 \text{ MeV}$) data, assuming $\Delta \text{NDTT} = d(\text{dpa})^{0.688}$ for $10 \leq \Delta \text{NDTT} \leq 300^\circ\text{F}$.

plotted and a straight line was constructed. And, finally the MTR data³ above $\Delta\text{NDTT} = 100^\circ\text{F}$ were plotted and a straight line was constructed. The indication is that the relatively high fast-flux data (ORR and MTR) are essentially parallel to the HFIR A350-LF3 data. This was used as justification for constructing a curve through the HFIR A212-B data points parallel to the A350-LF3 curve. The upper A212-B point was used as a conservative measure.

A comparison of the correlations represented by Figs. 3.1 and 3.2 (upper two curves) shows that Fig. 3.2 predicts less of a rate effect between ϕ ($E > 1.0$ MeV) = 2.4×10^8 and 1.2×10^9 neutrons/cm²·s. In this study, the authors have considered the correlation implied by Fig. 3.2 to provide the more accurate estimate of ΔNDTT for the LWR vessel supports.

One might argue that the small difference in fast fluxes corresponding to the HFIR A212-B and A350-LF3 irradiations (2.4×10^8 and 1.2×10^9 neutrons/cm²·s) relative to the factor of $\sim 10^5$ between HFIR and the MTRs ($\sim 10^8$ and 10^{13} neutrons/cm²·s) would not permit distinguishing between the two HFIR fluxes with regard to establishing a rate effect. However, Hamilton⁶ recently presented data that indicate essentially no rate effect in the fast flux ($E > 1.0$ MeV) range of 1×10^{10} to 3×10^{13} neutrons/cm²·s at $\sim 200^\circ\text{F}$. This indicates that the ΔNDTT differences observed between HFIR and the MTRs are associated with a rate effect below a fluence rate of $\sim 1 \times 10^{10}$ neutrons/cm²·s. Thus, for this study, the observed difference in ΔNDTT for fluxes of 2.4×10^8 and 1.2×10^9 neutrons/cm²·s were considered to be real. There is, however, an inconsistency with regard to the A105-II data: although these data correspond to an intermediate flux level (3 to 7×10^8 neutrons/cm²·s), they tend to coincide with the A350-LF3 data, which correspond to a higher flux (1.2×10^9 neutrons/cm²·s). Perhaps this implies that a rate effect is not discernible within the flux range of 2.4×10^8 to 1.2×10^9 neutrons/cm²·s. However, for this study the A105-II data were discounted insofar as establishing a rate effect.

Extrapolation and interpolation of the HFIR data for application to the support study were accomplished by assuming that $\text{dpa} \propto (\text{dpa rate})^\eta$ for a given value of ΔNDTT . Corresponding values of dpa, dpa rate, and ΔNDTT were taken from Figs. 3.1 and 3.2 to obtain the log-log plots in Figs. 3.3 and 3.4, respectively. As mentioned above, Fig. 3.2 and thus Fig. 3.4 are believed to be the more accurate representation of the trends. However, it must also be emphasized that in either case the values of $\Delta\text{NDTT} > 100^\circ\text{F}$ and values of dpa rate outside the range of 3.7×10^{-13} to 1.7×10^{-12} s⁻¹ represent extrapolations of the HFIR data.

Fig. 3.3. dpa ($E > 0.1$ MeV) vs dpa rate for specific values of ΔNDTT , based on data in Fig. 3.1.

Fig. 3.4. dpa ($E > 0.1$ MeV) vs dpa rate for specific values of ΔNDTT , based on data in Fig. 3.2.

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*Available for purchase from the National Technical Information Service, Springfield, Virginia 22161.

4. SURVEY OF SUPPORT DESIGN FEATURES

4.1 Introduction

A survey was made of LWR vessel supports using the file of final safety analysis reports (FSARs) located at the Nuclear Operations Analysis Center (NOAC) at ORNL. Because of limited time and resources, virtually no contacts were made with industry, that is, with nuclear steam system suppliers (NSSSs), architect-engineers (AE) organizations, or with utilities. The objective of this survey was twofold: (1) to determine if supports or any part thereof are susceptible to significant radiation damage and (2) to determine if the loading conditions and/or fabrication techniques would result in significant primary or secondary tensile stresses.

Two prior studies^{1,2} were very useful in elucidating, corroborating, and supplementing the FSAR data. For several nuclear plants the available data from all sources were too sketchy to be definitive. Consequently, the reader should understand that although most of the FSAR data are clear, there may be specific plants for which the inferences are based on meager data and may be incorrect. Nevertheless, for most plants the survey provides data that permit an understanding of the design rationale followed by NSSS and AE designers.

Of the 125 LWR plants surveyed, 96 plants were operating, with the remainder having received construction permits. Since the survey was completed, one of the plants, LaCrosse, has been decommissioned.

4.2 Categorization

The reactor vessel support designs for the 125 plants were divided into five broad categories. These categories, which are assigned to each plant in Table 4.1, include (1) skirt, (2) long column, (3) shield tank, (4) short column, and (5) suspension. All of the GE BWR plants (41), except two (Big Rock Point and LaCrosse), have skirt supports typically as shown in Fig. 4.1. (As noted previously, the LaCrosse plant has been decommissioned.)

Because the skirt supports are located remotely from the core with a large volume of intervening metal and water, radiation damage is not anticipated. In addition, the very large section modulus of the skirt's design is anticipated to result in very low stresses resulting from bending loads of all types. The Big Rock Plant reactor vessel, the only Category 5 plant in Table 4.1, has suspension supports as shown in Fig. 4.2. The suspension supports are designed to have significant primary tensile stresses and are also anticipated to reside within a zone of potential radiation damage.

Seven of the PWR plants have skirt supports for the reactor vessel (Fig. 4.3). The comments on the BWR skirt supports are believed to apply also to the PWR reactor vessel supports (i.e., insignificant radiation damage and low tensile-stress state for all loading conditions). Babcock and Wilcox (B&W) served as the NSSS on all seven of these plants.

Table 4.1 Basic type of support for LWR vessels

No.	Power station name	Type of plant	Type of support				
			Skirt	Long column	Shield tank	Short column	Suspension
			1	2	3	4	5
1	Arkansas Nuclear Unit 1	PWR	1				
2	Arkansas Nuclear Unit 2	PWR		2			
3	Beaver Valley Unit 1	PWR			3		
4	Big Rock Point Unit 1	BWR					5
5	Browns Ferry Unit 1	BWR	1				
6	Browns Ferry Unit 2	BWR	1				
7	Browns Ferry Unit 3	BWR	1				
8	Brunswick Unit 1	BWR	1				
9	Brunswick Unit 2	BWR	1				
10	Byron Unit 1	PWR				4E	
11	Callaway Unit 1	PWR				4C	
12	Calvert Cliffs Unit 1	PWR				4C	
13	Calvert Cliffs Unit 2	PWR				4C	
14	Catawba Unit 1	PWR				4G	
15	Cook Unit 1	PWR				4C	
16	Cook Unit 2	PWR				4C	
17	Cooper Station	BWR	1				
18	Crystal River Unit 3	PWR	1				
19	Davis-Besse Unit 1	PWR				4A	
20	Diablo Canyon Unit 1	PWR				4F	
21	Diablo Canyon Unit 2	PWR				4F	
22	Dresden Unit 2	BWR	1				
23	Dresden Unit 3	BWR	1				
24	Duane Arnold	BWR	1				
25	Farley Unit 1	PWR				4C	
26	Farley Unit 2	PWR				4C	
27	Fitzpatrick	BWR	1				
28	Fort Calhoun Unit 1	PWR				4C	
29	Fort St. Vrain	GCR		Not applicable			
30	Ginna	PWR				4C	
31	Grand Gulf Unit 1	BWR	1				
32	Haddam Neck	PWR				4C	
33	Hatch Unit 1	BWR	1				
34	Hatch Unit 2	BWR	1				
35	Indian Point Unit 2	PWR				4F	
36	Indian Point Unit 3	PWR				4F	
37	Kewaunee	PWR				4C	
38	La Crosse	BWR	1				
39	La Salle Unit 1	BWR	1				
40	La Salle Unit 2	BWR	1				
41	Limerick Unit 1	BWR	1				
42	Maine Yankee	PWR			3		

Table 4.1 (continued)

No.	Power station name	Type of plant	Type of support				
			Skirt	Long column	Shield tank	Short column	Suspension
			1	2	3	4	5
43	McGuire Unit 1	PWR				4G	
44	McGuire Unit 2	PWR				4G	
45	Millstone Unit 1	BWR	1				
46	Millstone Unit 2	PWR				4G	
47	Monticello	BWR	1				
48	Catawba Unit 2	PWR				4G	
49	Hope Creek Unit 1	BWR	1				
50	Millstone Unit 3	PWR			3		
51	Nine Mile Point Unit 1	BWR	1				
52	North Anna Unit 1	PWR			3		
53	North Anna Unit 2	PWR			3		
54	Oconee Unit 1	PWR	1				
55	Oconee Unit 2	PWR	1				
56	Oconee Unit 3	PWR	1				
57	Oyster Creek Unit 1	BWR	1				
58	Palisades	PWR				4D	
59	Palo Verde Unit 1	PWR		2			
60	Palo Verde Unit 2	PWR		2			
61	Peach Bottom Unit 2	BWR	1				
62	Peach Bottom Unit 3	BWR	1				
63	Pilgrim Unit 1	BWR	1				
64	Point Beach Unit 1	PWR				4F	
65	Point Beach Unit 2	PWR				4F	
66	Prairie Island Unit 1	PWR		2			
67	Prairie Island Unit 2	PWR		2			
68	Quad Cities Unit 1	BWR	1				
69	Quad Cities Unit 2	BWR	1				
70	Rancho Seco Unit 1	PWR	1				
71	River Bend Unit 1	BWR	1				
72	Robinson Unit 2	PWR				4C	
73	Salem Unit 1	PWR				4F	
74	Salem Unit 2	PWR				4F	
75	San Onofre Unit 1	PWR				4?	
76	San Onofre Unit 2	PWR		2			
77	San Onofre Unit 3	PWR		2			
78	Sequoyah Unit 1	PWR				4C	
79	Sequoyah Unit 2	PWR				4C	
80	St. Lucie Unit 1	PWR		2			
81	St. Lucie Unit 2	PWR		2			
82	Summer Unit 1	PWR				4C	
83	Surry Unit 1	PWR			3		
84	Surry Unit 2	PWR			3		

Table 4.1 (continued)

No.	Power station name	Type of plant	Type of support				
			Skirt	Long column	Shield tank	Short column	Suspension
			1	2	3	4	5
85	Susquehanna Unit 1	BWR	1				
86	Susquehanna Unit 1	BWR	1				
87	Three Mile Island Unit 1	PWR	1				
88	Trojan	PWR				4A	
89	Turkey Point Unit 3	PWR				4A	
90	Turkey Point Unit 4	PWR				4A	
91	Vermont Yankee Unit 1	BWR	1				
92	Washington Nuclear Unit 2	BWR	1				
93	Waterford Unit 3	PWR				4D	
94	Wolfe Creek Unit 1	PWR				4C	
95	Yankee-Rowe Unit 1	PWR				4B	
96	Zion Unit 1	PWR				4E	
97	Zion Unit 2	PWR				4E	
98	Vogtle Unit 1	PWR				4C	
99	Vogtle Unit 2	PWR				4C	
100	Beaver Valley Unit 2	PWR			3		
101	Bellefonte Unit 1	PWR				4D	
102	Bellefonte Unit 2	PWR				4D	
103	Braidwood Unit 1	PWR				4E	
104	Braidwood Unit 2	PWR				4E	
105	Byron Unit 2	PWR				4E	
106	Clinton Unit 1	BWR	1				
107	Comanche Peak Unit 1	PWR				4C	
108	Comanche Peak Unit 2	PWR				4C	
109	Fermi Unit 2	BWR	1				
110	Grand Gulf Unit 2	BWR	1				
111	Limerick Unit 2	BWR	1				
112	Nine Mile Point Unit 2	BWR	1				
113	Palo Verde Unit 3	PWR		2			
114	Perry Unit 1	PWR	1				
115	Perry Unit 2	PWR	1				
116	Seabrook Unit 1	PWR				4F	
117	Seabrook Unit 2	PWR				4F	
118	Shearon Harris Unit 1	PWR				4F	
119	Shearon Harris Unit 2	PWR				4F	
120	Shoreham	BWR	1				
121	South Texas Unit 1	PWR				4C	
122	South Texas Unit 2	PWR				4C	
123	Watts Bar Unit 1	PWR				4C	
124	Watts Bar Unit 2	PWR				4C	
125	Washington Nuclear Unit 1	PWR				4C	
126	Washington Nuclear Unit 3	PWR		2			

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Fig. 4.1. Skirt RPV support for BWRs.

Fig. 4.2. BWR (Big Rock Point) vessel with suspension supports.

Fig. 4.3. PWR vessel with skirt support.

Supports for 11 plants have been placed in the long-column category (Fig. 4.4). Small keyway gap size leads to the inference that tensile stresses in the column from all loadings should be low. On the other hand, the columns are located in a zone of potentially high radiation damage. For nine of the plants in this category, Combustion Engineering, Inc. (CE) acted as the NSSS, and for two of the plants, Westinghouse Electric Company (W) acted as the NSSS.

Eight plants have had the supports categorized as shield-tank type. For all of these plants, Stone & Webster has acted as the AE. W was the NSSS for seven plants, and CE for one of the plants. It is not completely clear from FSAR data if the supports for this latter plant should be classified as shield tank. Figure 4.5 shows design features typical of shield-tank-supported reactor vessels. It is evident that much of the structure would be highly susceptible to the radiation damage hypothesized herein. On the other hand, it would be anticipated that the structure would be quite stiff and consequently have very low primary stresses from all loading cases. It is uncertain as to whether

Fig. 4.4. PWR vessel with long-column supports.

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Fig. 4.5. PWR vessel with shield-tank support.

secondary stresses (e.g., thermal or residual) could have a significant role in a fracture analysis of a support of this type.

Category 4 (short column), which includes the largest number of PWR plants (58), is an assemblage of designs with two principal commonalities: (1) loads, vertical and horizontal, are transmitted from the vessel nozzles (and vessel brackets in a few instances) through a radially keyed structure to the biological-shield wall and (2) none of the structural components within the reactor vessel biological-shield annulus extends below the core midplane.

In general, the biological-shield wall of most plants is designed for a maximum temperature of 150°F for protection of the concrete. As a consequence, many of the Category 4 supports, because of the short heat-flow path from reactor vessel nozzle to biological-shield wall, have special designs for heat dissipation. In particular, many of the short-column designs are weldments with openings to promote natural-convection cooling or to assist forced-convection cooling. Others use cooling coils with water as the cooling medium. Because of the large variation in designs of these structures few generalizations can be made. They appear to have a large variability in susceptibility to radiation damage because of the large variability in vertical location relative to the reactor core. On the other hand, designs with relatively broad cavities between the reactor vessel and biological shield are subject to neutron streaming that elevates the fluence experienced for a significant distance above the top of the core.

Because of inferred design characteristics that would promote significant differences in radiation-damage susceptibility or in bending or primary tensile-stress fields, Category 4 was subdivided into subgroups 4A through 4C. Figure 4.6 provides several views and details of a PWR vessel with a Category 4A support having some features that are similar to the Trojan and Turkey Point plants, which were selected for detailed evaluations (Chaps. 5, 6, and 7). In this support category, vertical loads are transmitted from vessel nozzle weld pads through a short-keyed pedestal or girder to steel beams cantilevered radially inward from the biological-shield wall, and horizontal loads are transmitted through the same load path or to separate structures. The radial beams of this category are susceptible to potential radiation damage and are designed to resist vertical loading with primary bending stresses.

Figure 4.7 is a sectional elevation view of a PWR vessel with a Category 4B support. In this design, loads are transferred from vessel brackets to a ring girder weldment that is supported from the biological shield. Because the loadings are resisted by the girder by means of a torsional moment, primary bending stresses are induced. The girder is located below the top of the core but tends to be shielded by the adjacent suspended neutron shield tank. Thus, the potential for radiation damage is unclear.

Figure 4.8 provides perspective and elevation views of a PWR vessel equipped with a Category 4C support. Vertical loads pass from the vessel nozzle weld pads through radially keyed shoes to short weldment pedestals anchored on the bottom surfaces to recesses in the biological shield. Horizontal loadings are resisted by bending and shear stresses in the shoes and pedestal weldments. Insufficient information is available for assessment of potential secondary tensile stresses, that is,

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Fig. 4.6. PWR vessel with category 4A support.

Fig. 4.7. PWR vessel with category 4B support.

Fig. 4.8. PWR vessel with category 4C support.

from thermal or residual stress sources. Because of the short height, heat transfer provisions (vent openings and water cooling) are incorporated in the design to promote rapid cooling. Radiation-damage susceptibility is among the lowest of Category 4 because of the short height generally employed. This design concept has the highest frequency of use within Category 4 with its employment in 25 plants.

Figure 4.9 provides a section elevation view of a PWR vessel equipped with Category 4D supports. Features are similar to Category 4E with the employment of short pedestal weldments to transfer vessel loadings to either reinforced concrete corbels or structural-steel brackets cantilevered radially inward from the biological shield. Except for the uncertainty as to whether structural-steel brackets are actually used,

Fig. 4.9. PWR vessel with category 4D support.

the susceptibility to radiation damage and the level of primary and secondary stresses would appear to be similar to Category 4C.

Figure 4.10 provides plan and elevation views of a PWR vessel equipped with a Category 4E support. Provisions are similar to a Category 4C support except for the incorporation of brackets at the level of the radially keyed shoe to resist horizontal loadings. The susceptibility to radiation damage would appear to be similar to Category 4C.

Figure 4.11 provides perspective and section elevation views of a PWR vessel equipped with a Category 4F support. With this support scheme, vertical and horizontal loads are transmitted through nozzle weld pads to radial keys mounted on a structural-steel ring girder that is in turn mounted on short weldment pedestals. In some cases the relative position of the ring girder and short weldment pedestals are inverted. The distinguishing feature of this category, relative to Category 4C, is the employment of a structural-steel ring girder for

Fig. 4.10. PWR vessel with category 4E support.

transference of horizontal loadings. Otherwise, the conclusions with regard to radiation-damage susceptibility and stress state would appear to be the same.

Figure 4.12 provides a section elevation view of a PWR vessel equipped with a Category 4G support. Features of this support are similar to Category 4C except that horizontal loading is transferred directly to the reinforced concrete structure of the biological shield. The sides, as well as the bottom of the recess in the biological shield wall, provide for load transference; consequently, no significant primary tensile- or bending-stress fields should be generated within the support structure.

A fundamental objective of the design of all PWR support systems is the incorporation of guides to establish a fixed reactor vessel center-line for all operating, upset, and faulted conditions. In general, the vertical loadings on the supports are applied to components designed to accommodate radial thermal expansion by sliding. Two exceptions to this method of accommodating radial thermal expansion are the provisions of pivoting columns in the Trojan supports and nested rollers in the Turkey Point supports. As noted by Hopkins,¹ the NSSS and AE designers have assumed that the imposed forces on the supports resulting from differential friction effects are negligible. The FSAR data on supports verify

Fig. 4.11. PWR vessel with category 4F support.

Fig. 4.12. PWR vessel with category 4G support.

this observation. ORNL is not aware of any attempt to examine loadings that would result from differential friction effects.

References

1. W. G. Hopkins, Idaho National Engineering Laboratory, *Reactor Pressure Vessel Supports for Pressurized Water Reactors and Boiling Water Reactors, Residual Life Assessment of Major Light Water Reactor Components - Overview Volume 1*, NUREG/CR-4731 (EGG-2469, Vol. 1), June 1987.*
2. G. A. Knorovski, R. D. Krieg, and G. C. Allen, Jr., Sandia National Laboratories, *Fracture Toughness of PWR Component Support*, NUREG/CR-3009 (SAND78-2347), February 1983.*

*Available for purchase from National Technical Information Service, Springfield, Virginia 22161.

5. SELECTION OF LWR PLANTS FOR SPECIFIC-PLANT ANALYSIS

The criteria established for selection of two plants for specific-plant analysis are as follows:

The plants should be among those having a relatively high potential for vessel-support failure as a result of radiation-enhanced propagation of flaws. Compliance with this condition was judged on the basis of the following considerations:

1. A potentially critical portion of the support should be exposed to the relatively high-neutron-flux regions of the cavity (from mid-height to the end of the core).
2. The support material should have a relatively high potential for radiation embrittlement.
3. The portion of the support within the high-flux region should be subjected to relatively high tensile stresses, primary-load tensile stresses being of particular concern. Secondary tensile stresses (thermal and residual) are also of concern.
4. The potentially critical portion of the support should have a relatively high potential for flaws of critical size.

Information in Table 3.5 indicates that the cavity fluxes for the BWRs are much less than those for PWRs; as indicated in Chap. 4, BWR vessels are supported on skirts that are far removed from the bottom end of the core.* Thus, BWRs were excluded from consideration. All but one of the B&W PWRs are supported on skirts, and they also were excluded from consideration.

About 10% of the PWR vessels are supported on long columns and another 10% on shield tanks that extend the length of the core and thus are exposed to the maximum flux. At the outset of this study each of the PWR NSSS vendors and the Electric Power Research Institute (EPRI) were contacted informally and given the opportunity to contribute to the study. Combustion Engineering (CE) and EPRI responded informally with updated analyses of the long columns (CE) and shield tanks (EPRI and Stone and Webster), considering the HFIR surveillance data. The preliminary indication was that critical flaw sizes corresponding to 32 EFPY were "acceptably" large. Thus, these supports also were excluded from consideration in this study. (ORNL has not reviewed the industry evaluations in detail and thus is not in a position to comment on the industry conclusion.)

The remaining PWR vessel supports fall in the short-column category (Chap. 4). As one extreme, this category includes very short, stubby supports (columns) that rest directly on the concrete biological shield at an elevation above the upper end of the core where the flux is relatively low (Fig. 5.1), and the other extreme is columns extending to about midheight of the core and resting on steel cantilever beams. As indicated in Chap. 4, only Trojan and perhaps Davis Besse are of this

*Big Rock Point is an exception but was not considered because of its small size.

Fig. 5.1. Relative axial variation of fast neutron ($E > 1.0$ MeV) flux and fluence within pressure vessel wall.

latter type, while Turkey Point Units 3 and 4 (identical supports) are similar to Trojan but with the steel cantilever beam located closer to the top of the core, where the flux is somewhat less. Of all plants in the short-column category, Trojan and Turkey Point appeared to have the greatest potential for fracture-related failure of the vessel supports. Both designs include cantilever beams in high-flux regions of the cavity, and both cantilevers are stressed in tension by primary loads. As mentioned in Chap. 4, Trojan and Turkey Point Unit 3 were selected for the specific-plant analyses in this study.

6. BRITTLE FRACTURE EVALUATION OF TROJAN NUCLEAR PLANT REACTOR PRESSURE VESSEL SUPPORTS

6.1 Introduction and Summary

The Trojan vessel-support structure is comprised of upper components that resist horizontal loads, lower components (cantilever beams) that resist vertical loads, and pinned columns that transfer vertical loads from the vessel to the lower components. Only the cantilever beam is considered in this study because this component of the structure is subjected to a relatively high fast-neutron fluence, and the applied loads result in primary tensile stresses. Several potentially limiting locations on the lower support beam are within the portion of the assembly embedded in the concrete biological shield. A beam-on-elastic-foundation-analysis model provided a detailed definition of stresses at these locations.

Stress analyses of the support beam, and postulated flaws within it, were planned in the form of a parametric study. This approach permitted a broad evaluation of the potential impact of premature embrittlement on the support beam structural integrity. Parameters included in the study were locations in the beam (3), loading conditions (4), beam support configurations (2), flaw aspect ratios (5), flaw depths (6), and plant operating times (3).

The support beam weldments are fabricated from A36 material. Fracture-toughness properties of this material have been shown to be strain-rate sensitive. Materials data were taken from the open literature and converted to a strain rate appropriate to the dynamic response of the reactor vessel on its supports. Equations were fitted to the data, and a lower bound design curve was defined.

Irradiation-induced shifts in nil ductility transition temperature (NDTT) were calculated using correlations developed from the High Flux Isotope Reactor (HFIR) data (Chap. 3). Analysis of the plant operating profile gave radiation-induced NDTT shifts at the critical location within the beam ranging from 30°F at the present time to 75°F at a plant operating time of 32 effective full-power years (EFPY). Strain rates produced by the vessel system response to accident loading produce a further 60°F NDTT shift (relative to strain rates associated with slow bend tests). Design fracture-toughness values at the beam locations selected for analysis ranged from a high of 49 ksi·√in. to a low of 31 ksi·√in. at 32 EFPY.

Dynamic fracture-toughness limits were superposed on results from the stress-intensity-factor analyses to generate curves of critical flaw depth as a function of loading applied to the vessel support. Critical flaw depths obtained for the maximum loading condition [small-break loss-of-coolant accident (SBLOCA)] are summarized in the following table.

Beam location	Loading	Critical flaw depth (in.)		Flaw length at beam surface (in.)
		7.48 ^a EFPY	32 EFPY	
Interface with the reactor cavity	SBLOCA	0.89	0.68	Full flange width (16 in.)
Maximum bending moment location	SBLOCA	0.88	0.61	Full flange width (16 in.)
Flange grout hole	SBLOCA	>2.0	0.42	Full flange depth (2.5 in.)

^aCurrent condition (late 1988).

The grout hole, which is 4.0 in. in diameter and located in the upper flange of the embedded portion of the beam ~11 in. from the inner surface of the biological shield, results in the smallest critical flaw size for times approaching 32 EFY.

Factors with the potential for influencing the calculated critical flaw depths in the beam flange are (1) refinement of the stress-intensity-factor correlation to include shear-lag effects, (2) variations in the material NDTT, (3) residual stress effects, and (4) inclusion of support for the beams from concrete inboard of the inner support pedestal. Pilot evaluations of these effects, included in this report, indicate that their combined effect will be to produce critical flaw sizes that are not greater than those given in the summary table.

Fatigue-induced flaw growth makes a negligible contribution to the above critical flaw dimensions. The flaws would thus have to have been present at final inspection of the support structure to constitute a brittle fracture hazard.

The question regarding the existence of flaws of critical size has not been addressed in this study. Note, however, that at the two locations other than the grout hole, the surface length of the critical flaw is the full width of the flange (16 in.). It seems that a flaw of this size would be readily detectable at the time of fabrication.

Detection of the critical flaw in the edge of the grout hole appears less likely because the flaw is smaller (0.4 × 2.5 in.), and the hole was flame-cut and dressed, a process normally resulting in a rather rough surface.

In-service inspection for flaws in the lower support beam may be impractical, particularly for the portion of the beam embedded in the concrete biological shield. Inspection records, however, may shed light on the likelihood of significant flaws existing [utility records were not available to Oak Ridge National Laboratory (ORNL) at the time of this study].

Principal uncertainties in the flaw tolerance analysis of the support beams are related to (1) the fracture-toughness data for A36 at

strain rates and temperature appropriate to the support beam loading conditions, (2) the correlation used for applying the HFIR embrittlement data to the evaluation of PWR vessel support, (3) the initial NDTT for the material, (4) the operating temperatures of the support, and (5) the magnitude of residual stresses. There is also a considerable uncertainty regarding the existence of flaws in the beams.

6.2 Scope and Objective

The Trojan vessel-support structure¹ is comprised of upper and lower beam structures joined by pinned columns (Fig. 6.1). This analysis considers only the lower beam because (1) it is loaded to give some of the highest bending-induced tensile stresses in the support and (2) it is the component located closest to the core midplane, where the fast-neutron fluence is nearly a maximum.

At the time of this analysis, no data were available on the geometric characteristics of potential flaws in the support beams. The analysis was therefore performed as a parametric study with a range of flaw depths and aspect ratios considered. The analysis objective was to define the critical flaw size envelope for the high-stress and/or high-fluence locations within the support beam. It is intended that this

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Fig. 6.1. Radial section through Trojan reactor vessel supports showing principal structural and kinematic elements.

envelope be used as a basis for evaluating the postulated beam flaws, when a definition of the flaw population becomes available.

A critical-flaw-size analysis was performed for three locations on the beam. These locations and the bases for their selection were as follows:

1. Interface with the reactor cavity liner: a transverse fillet weld between the beam flange and the cavity liner exists at this location. A potential exists therefore for weld-induced cracking over the full width of the flange. This location also experiences the maximum radiation-induced displacements per atom (dpa) and dpa rate of all three locations considered.

2. Location of the beam maximum bending moment: the beam is supported on pedestals and is cast in concrete from the cavity liner interface back into the cavity wall. This type of support can be characterized as a beam-on-elastic-foundation support in which the support stiffness varies along the length of the beam. With this type of support, the peak bending moment (and the associated beam flange primary bending stress) is located a short distance back from the start of the elastic foundation support. This location is clearly one of the critical locations on the beam.

3. Four-inch-diameter beam flange grout holes: these holes are located in both the upper and lower beam flanges, directly over the centerline of the inner support pedestal. While not necessarily located at the point of maximum bending moment, they are close to it. This fact, coupled with the loss of 25% of the flange area and the introduction of a geometric stress concentration ($K_T = 2.43$), makes the flange hole a potentially limiting location. The fact that the flange holes were flame-cut adds to the potential for the introduction of undetected flaws during the fabrication process.

A further parameter considered in the analysis was the effectiveness of that portion of the concrete inboard of the inner support pedestal inner flange. This portion of the concrete foundation would be expected to experience some of the highest elastic foundation compression stresses and could potentially be loaded beyond its crushing strength. In addition, some uncertainty exists relative to the degree of fit achieved between the concrete and the support beam flanges at this critical location. To accommodate these potential effects, all analyses were run both with and without the inner concrete included in the foundation model.

Four loading cases were considered in the analysis. Load sources contributing to the support loads were dead weight (DW), pipe thermal thrusts (T), the operating basis earthquake (OBE), the safe shutdown earthquake (SSE), and both small- and large-break loss-of-coolant accidents (SBLOCA and LBLOCA). Specific load combinations considered in the analysis were as follows:

$$\text{Normal + upset} = \text{DW} + \text{T} \pm \text{OBE}$$

$$\text{Faulted (1)} = \text{DW} + \text{T} \pm \text{SSE}$$

$$\text{Faulted (2)} = \text{DW} + \text{T} \pm \text{SBLOCA}$$

$$\text{Faulted (3)} = \text{DW} + \text{T} \pm \text{LBLOCA}$$

Subsequent to the initiation of the analysis, Oak Ridge National Laboratory (ORNL) was informed that Nuclear Regulatory Commission (NRC) had approved the Trojan leak-before-break analysis, thereby eliminating the double-ended pipe-break (LBLOCA) from the plant faulted condition transient loading list. Analysis was completed for the LBLOCA condition, however, because the addition of this fourth loading-condition result improves the definition of load-dependent variables and thus improves the accuracy of interpolation between the load cases considered. In this connection, note that the critical flaw size is a nonlinear function of the applied stress.

The SBLOCA loading condition provided by the utility constitutes a generic, bounding case that involves failure of auxiliary coolant lines and thus represents a more severe loading condition than often referred to as SBLOCA.

Three specific times in the life of the plant were considered in the analysis: (1) start of life, (2) current condition (late 1988, 7.48 EFPY), and (3) 32 EFPY, which is the approximate present license period. Interest in the start-of-life condition stems from the strain-rate sensitivity of the fracture-toughness data for A36.

In summary, the scope of the support beam parametric study was as follows:

<u>Parameter</u>	<u>Number of cases</u>
Beam locations	3
Loading conditions	4
Beam support configurations	2
Flaw aspect ratios	5
Flaw depths	6
Reactor operating periods	3

6.3 Loading and Operating Environment

All loading cases have been taken from (1) the minutes of the June 29-30, 1988, Pittsburgh meeting² of Portland General Electric (PGE), Westinghouse, NRC, and ORNL and (2) the PGE letter of August 2, 1988.³

6.3.1 Normal plus upset loading

Maximum load/support for T + DW + OBE

$$P_1 = 288 + 530 + 281 = 1099 \text{ kips.}$$

6.3.2 Faulted conditions

1. SSE

Maximum load/support for T + DW + SSE = P_1

$$P_1 = 288 + 530 + 394 = 1212 \text{ kips.}$$

2. SBLOCA

Maximum load/support for T + DW + SBLOCA = P_2

$$P_2 = 288 + 530 + 740 = 1558 \text{ kips.}$$

3. LBLOCA

Maximum load/support for T + DW + LBLOCA = P_3

$$P_3 = 288 + 530 + 1625 = 2443 \text{ kips.}$$

6.3.3 Cyclic loading

Event	$P_{\max}$ (kips)	$P_{\min}$ (kips)	Number of cycles
Refueling	818	395	40
OBE	1099	537	400
SSE	1212	424	20
SBLOCA	1558	78	5

6.3.4 Operating environment

Beam temperature $\approx 90^\circ\text{F}$.

6.3.5 Load combinations

Deterministic load combinations are used in Chap. 6 to define the load-time-history required as input to the strain-rate element of the material fracture-toughness evaluation. Probabilistic combinations of the SSE and SBLOCA loads will produce a peak load 6.3% higher than that given in Sect. 6.3.2. Plots of critical flaw depth as a function of support load are included to permit an approximate evaluation of the impact of this load increment. However, these plots do not reflect the potential increase in straining rate possible with the combined SSE/SBLOCA loading and the associated influence of straining rate on the support material fracture toughness.

6.4 Support Beam Analysis

6.4.1 Analytical model

An overview of the support beam geometry is given in Fig. 6.1, with additional details of the structural members given in Figs. 6.2 and 6.3.

Vertical loading is applied to lugs on the inboard end of the beam via pins and vertical links. Lateral shear loading is reacted by a

Fig. 6.2. Geometry and dimensions of Trojan reactor vessel support beams.

Fig. 6.3. Cross-section geometry and dimensions for Trojan reactor vessel support beam pedestals. (a) Inner pedestal, (b) outer pedestal.

separate structure located just below the elevation of the vessel nozzles. Loads applied to the support beam thus remain vertical for all loading cases.

Reaction to the applied loads is provided by (1) support pedestals built into the cavity wall and (2) interaction directly with the cavity wall concrete. It was considered important to model these interactions as accurately as possible because the modeling assumptions could have a significant influence on the magnitude and distribution of bending moments in that portion of the beam located within the cavity wall. Note that the 4-in.-diam holes in the beam flanges are located in that portion of the beam within the cavity wall.

A beam on the elastic-foundation model was selected on the basis of its ability to provide a detailed definition of the bending moment distribution within the cavity wall. Rather than use the conventional

approach to beam-on-elastic-foundation analysis, it was decided to model the support as a series of closely spaced struts with prototypic axial stiffness and very low bending stiffness. This approach permitted a detailed representation of the distribution of support stiffness as the beam passed over the various elements of the support pedestals.

The analytical model for both the support beam and the foundation elements is shown in Fig. 6.4, together with a sketch of the beam and its support pedestals. Node points and numbers are indicated on both the model and the sketch to aid in interpretation of the analysis results. The model beam numbering system is illustrated in Fig. 6.5(a) together with details of the model in Fig. 6.5(b), showing beam and node numbers near the location of the flange holes. Note that the flexural rigidity of the vertical foundation members is set at a very low value, thereby ensuring that they take only axial loading.

Fig. 6.4. Trojan reactor vessel support beam beam-on-elastic foundation analytical model, showing locations of nodes on model and in structure. (a) Analytical model node numbers, (b) node locations on support beam and its foundation.

Fig. 6.5. Details of the Trojan reactor vessel-support-beam analytical model at the beam-flange grout-hole location. (a) Model beam numbers (pin joints on vertical foundation beams), (b) model details at the flange hole.

6.4.2 Model element properties

6.4.2.1 Beam (solid section). See Fig. 6.2 for the section geometry.

$$\begin{aligned}
 A_1 &= 16 \times 2.5 \times 2 + 13 \times 1.75 \times 2 \\
 &= 125.5 \text{ in.}^2
 \end{aligned}$$

$$I_1 = \frac{(16 \times 18^3 - 12.5 \times 13^3)}{12}$$

$$= 5487.4 \text{ in.}^4$$

6.4.2.2 Beam (section with 4-in.-diam holes)

$$A_2 = 125.5 - 2 \times 4 \times 2.5 = 105.5 \text{ in.}^2$$

$$I_2 = 5487.4 - 2 \times 4 \times 2.5 \times 7.75^2 - 2 \times 4 \times 2.5^3/12$$

$$= 4275.8 \text{ in.}^4$$

6.4.2.3 Concrete foundation. Assume that compressive loads from the beam flange diffuse into the concrete such that the effective boundary of the concrete column projects $\pm 30^\circ$ from verticals drawn from the edges of the beam flange. The foundation depth is taken as the dimension from the underside of the beam to the lower surface of the support column base plate.

Column width at top = flange width = 16.0 in.

$$\text{Column width at bottom} = 16 + 2 \times 29 \times \tan 30^\circ$$

$$= 49.5 \text{ in.}$$

The average concrete support column area for a unit length of support beam is thus obtained as

$$A_c = (16.0 + 49.5)/2 = 32.75 \text{ in.}^2/\text{in.}$$

$$E_c = 6 \times 10^6 \text{ lb/in.}^2 \text{ based upon aging in excess of 5 years}$$

(Ref. 4 and Appendix 5).

Note that all foundation areas are calculated in terms of square inch per inch of beam length. Each vertical support member represents the support provided by a segment of the foundation extending equal distances on either side of the beam/foundation member interaction node point. The unit length support areas are multiplied by the appropriate foundation lengths to obtain the area for a given foundation support element.

The concrete foundation elements can react loads only in the compression mode. Concrete foundation material exists, however, both above and below the support beams, thereby giving the concrete elements the capability of providing reaction loads in both directions. In the model, this condition is simulated by giving these foundation elements both tension and compression capability.

6.4.2.4 Flange of the front support pedestal. See Fig. 6.3(a) for geometry.

$$A = 16.0 \text{ in.}^2/\text{in.}$$

$$E = 29.0 \times 10^6 \text{ lb/in.}^2$$

6.4.2.5 Front support pedestal web. The web steel and the surrounding concrete both provide support to the beam in this segment of the foundation. The foundation area per unit length will thus be expressed in terms of an equivalent concrete area with a stiffness equal to that of the steel-concrete combination.

$$A_{\text{steel}} = A_s = 2 \times 1.5 = 3.0 \text{ in.}^2/\text{in.}$$

$$A_{\text{concrete}} = A_c = 32.75 - 3.0 = 29.75 \text{ in.}^2/\text{in.}$$

$$A_{\text{equivalent}} = A_E = A_c + A_s \cdot E_s/E_c$$

$$\therefore A_c = 29.75 + 3.0 \times 29/6 = 44.25 \text{ in.}^2/\text{in.}$$

6.4.2.6 Flange section of the rear support. Section dimensions shown in Fig. 6.3(b) for the rear support were taken from Ref. 5.

$$A = 15.5 \text{ in.}^2/\text{in.}$$

6.4.2.7 Web section of the rear support. This is another area where both the web steel and the surrounding concrete provide foundation support. The equivalent concrete formulation of Sect. 6.4.2.5 is used.

$$A = (32.75 - 0.688) + 0.688 \times 29/6 = 35.39 \text{ in.}^2/\text{in.}$$

6.4.2.8 Foundation modeling sensitivity assessment. It is evident from the previous sections that a number of assumptions are built into the modeling of the beam-elastic-foundation support. A sensitivity analysis was performed to determine the degree to which these assumptions influence the distribution and magnitude of the calculated bending moments in the support beams. Parameters included in this sensitivity study were (1) representation of the concrete reinforcing steel, (2) the concrete elastic modulus, and (3) representation of the concrete inboard of the inner support pedestal.

Results from this study showed that inclusion of the inner concrete in the foundation model could decrease the peak bending moment in the support beams by up to 13%. This segment of the foundation was therefore included as a variable in the parametric analysis of the support beams. The other foundation modeling variables, however, were found to influence the support beam bending moments by <1%. These variables were not, therefore, included in the support beam parametric analysis.

Further details of the beam-elastic-foundation modeling sensitivity analysis can be found in Appendix 5.

6.4.3 Beam analysis input

The unit length foundation areas were first converted to foundation member areas by multiplying the unit length areas by the foundation length represented by the foundation support members. This operation is summarized in Table A of Appendix 1. Elastic moduli associated with each of the foundation elements are also listed in Table A.

A unit load (200-kip) beam bending analysis was run using the Microsafe -2D finite-element program.⁶ The input data file for this analysis is given in Table B of Appendix 1. Note that each support incorporates two beams (see Fig. 6.2). The unit load of 200 kips thus corresponds to a load of 100 kips on each beam.

Analyses were run for two foundation support configurations. In the first configuration, the effective area of foundation element No. 21 [extending between nodes 2 and 22 in Fig. 6.4(a)] was input at a value of 108.4 in.² (see Table A of Appendix 1). This condition represents the concrete inboard of the inner support pedestal remaining fully effective throughout all loading events. In the second configuration the area of foundation element No. 21 was reduced to near zero. This represents the other limiting boundary condition in which the inner concrete segment is assumed to have failed locally in compression and so becomes ineffective as a foundation element. The version of the input file given in Table B of Appendix 1 represents the condition where element 21 is not providing support.

6.4.4 Beam analysis output

Output files from the two beam/support configurations analyzed are provided in Tables C and D of Appendix 1. Note that only load and moment data from these tables are used in the subsequent analysis of postulated flaws in the beams. The beam stresses calculated by the program are not used because the beam element in the program has a solid rectangular section, which is not representative of the Trojan support beams.

6.4.4.1 Effectiveness of the inner concrete support. Table E of Appendix 1 gives the foundation loading for the unit load case (loads are given for a single beam). The load per inch in the region inboard of the inner support pedestal is 13,317 lb/in. The width of the lower beam flange is 16.0 in. The bearing stress produced by the 200-kip unit load case is thus obtained as

$$\sigma_e = 13,317/16 = 832 \text{ lb/in.}^2 .$$

Bearing (compressive) stresses in the upper portions of the inner concrete can be obtained by factoring the unit load bearing stress by F, where F = load case maximum load/unit load. The ultimate compressive stress for the fully aged concrete is taken as 6.0 kips/in.² (Ref. 4).

The inner concrete reserve factor is defined as

$$RF = \frac{\text{allowable stress}}{\text{applied stress}} .$$

The inner concrete is taken to be effective in providing support for the beam for all cases in which $RF \geq 1.0$. Using the load case maximum loads from Sect. 6.3 in conjunction with the unit load concrete compressive stress results, the effectiveness of the inner concrete in the four loading cases considered is assessed as follows:

Load base	Maximum load $P_{\max}$ (kips)	F ($P_{\max}/200$)	Maximum concrete stress ($832 \times F$)	Inner concrete RF	Inner concrete effective ?
DW + T + OBE	1,099	5.50	4,576	1.31	Yes
DW + T + SSE	1,212	6.06	5,042	1.19	Yes
DW + T + SBLOCA	1,558	7.79	6,481	0.93	Marginal
DW + T + LBLOCA	2,443	12.22	10,167	0.59	No

Based upon these results, the inner concrete could be considered effective in supporting the beam for the seismic events but not for the LBLOCA event. The SBLOCA event represents a transition condition in which the inner concrete will provide something less than full support. All subsequent analyses, however, considered both inner concrete support conditions because this provides a basis for assessing issues relating to clearances between the concrete and the beam.

The unit load foundation reaction distribution is shown in Fig. 6.6. Reaction loads peak over the inner flange of the inner support pedestal. Compressive loads in that portion of the concrete located outboard of the inner support pedestal are substantially lower than those for the inboard concrete. Some slightly higher foundation reaction loads occur within the web area of the inner support pedestal for the case of no inner concrete support, but these occur in a region of the foundation reinforced by the pedestal webs and are not considered limiting.

6.4.4.2 Beam bending moments. Beam bending moment diagrams for the 200-kip unit loading condition are plotted in Fig. 6.7 for both inner concrete support conditions. Note that the presence of the inner concrete support has the effect of reducing the peak (Node 4) bending moment by 12.7%.

The bending moment diagrams of Fig. 6.7 are of interest because they provide a measure of the impact of simplifying assumptions that are sometimes incorporated into the analyses of partially embedded beams. The assumption that the maximum bending moment in an embedded cantilever occurs at the interface with the concrete would have produced an error in the estimate of the maximum bending moment of -27%. Conversely, the assumption of zero distributed support from the concrete (e.g., the beam being simply supported at the centers of its support pedestals) would

Fig. 6.6. Elastic-foundation reaction loads produced by 200-kip "unit load" applied to lugs of Trojan reactor vessel support beams.

Fig. 6.7. Bending-moment distributions in Trojan reactor vessel support beams produced by 200-kip "unit load" applied at support beam lugs.

have produced an overestimate of the peak bending moment of 57.5%. It would be fairly standard civil engineering practice, however, to select the inner face of the front support column as the location of maximum moment, in which case the error in maximum bending moment would be ~11% (low). These results suggest that modeling of the distributed foundation support may in general be necessary for the evaluation of any postulated flaws located within the embedded length of the support beam.

Bending moments are required from the unit load case for input into the flaw tolerance analysis. Figure 6.7 shows the location on the bending moment diagrams of the three locations selected for flaw tolerance analyses. Bending moments at these locations are as follows:

Location on beam	Inner concrete effective ?	Unit load (200-kip) bending moment (lb in.)
Cavity liner interface	NA	0.969×10^6
Node 4: maximum bending moment	Yes	1.333×10^6
Node 4: maximum bending moment	No	1.527×10^6
Flange hole centerline	Yes	1.238×10^6
Flange hole centerline	No	1.493×10^6

6.4.4.3 Support stiffness. OBE, SSE, and both SBLOCA and LBLOCA loads are all the product of dynamic analyses in which the vessel-support stiffness is a critical element. It is appropriate therefore to check the support stiffness given by the beam-on-elastic-foundation model with that used in the dynamic load analysis to verify compatibility of the models. The support stiffness used in the vessel dynamic response analysis was obtained from Ref. 2 as 1.94×10^4 kip/in.

Deflections of the beam under the 200-kip unit load are plotted in Fig. 6.8. The tip deflection for the case in which the inner concrete is effective is 0.00437 in. This translates to a stiffness K of

$$K = 200/0.00437 = 4.58 \times 10^4 \text{ kip/in.}$$

The stiffness value in Ref. 2 includes elements of the support structure (links, pins, shoes, etc.) that would introduce additional flexibility but are not included in this calculation. The calculated beam model stiffness is therefore considered to be compatible with that used in the original generation of the support dynamic loads.

Fig. 6.8. Deflection distributions for Trojan reactor vessel support beams produced by 200-kip "unit load" applied at support beam lugs.

6.5. Material Fracture Toughness

Fracture-toughness data for A36 in the unirradiated condition were taken from Ref. 7. These data are reproduced in Fig. 6.9.

A number of operations must be performed on the data of Fig. 6.9 to prepare a dynamic fracture-toughness curve suitable for a fracture mechanics evaluation of the vessel-support beam. As a prelude to performing these operations, the data of Fig. 6.9 were first converted into digital form and entered into a Lotus 123 spreadsheet. Figure 6.10 is a plot of the data entered into the spreadsheet. A visual comparison of Figs. 6.9 and 6.10 was used to verify the accuracy of the data transfer. Note that the two data points from Fig. 6.9, for which precise strain rates were not available, were not used in the analysis.

6.5.1 Strain-rate correction

The data of Fig. 6.9 show A36 to be a strain-rate sensitive material. The test temperature required to achieve a fracture toughness of $40 \text{ ksi}\cdot\sqrt{\text{in.}}$ increases by $\sim 150^\circ\text{F}$ as the test straining rate increases

Fig. 6.9. Fracture-toughness data for A36 steel showing temperature dependence and strain-rate sensitivity.

Fig. 6.10. Fracture-toughness data for A36 before transposition to prototypic reactor vessel support beam straining rate.

from 10^{-5} in./in./s to 10^{+1} in./in./s. It follows, therefore, that the fracture-toughness data used to evaluate the support beam flaw tolerance must be adjusted to be compatible with the vessel-support straining rate. The temperature shift relationship required to accomplish this adjustment is as follows.⁸

$$\Delta T = (150 - \sigma_{ys}) \dot{\epsilon}^{0.17} , \quad (1)$$

where

ΔT = temperature shift relative to static data ($^{\circ}\text{F}$),

σ_{ys} = room temperature yield strength (ksi),

$\dot{\epsilon}$ = strain rate (in./in./s).

The strain rate is calculated for a point on the elastic-plastic boundary for the crack tip according to the following equation:

$$\dot{\epsilon} = \frac{2\sigma_{ys}}{tE} , \quad (2)$$

where t is the loading time and E is the elastic modulus for the material.

The static yield stress for the vessel support beam material was obtained from Ref. 2 as 40.0 ksi. This value has been used in all of the support beam calculations. The beam material yield stress is close to the mean yield stress value given for A36 in Ref. 7 (38.1 ksi) and should therefore be representative of the test material yield stress.

All of the vessel-support loading cases considered involve a substantial increment of dynamic load, generated by either a seismic or a loss-of-coolant accident event. Loading rates for this dynamic component of the support load are dictated by the dynamic response of the vessel, which is dominated by the first rocking mode frequency of the vessel on its supports. The vessel first-mode frequency was obtained from Ref. 9 as 20 cps.*

It is evident from examination of the cyclic loading data of Sect. 6.3.3 that the loading for OBE, SSE, and SBLOCA events cycles between two positive values. The loading time for those events is therefore one-half the vessel first-mode vibration period.

$$t = 1/(20 \times 2) = 0.025 \text{ s} .$$

*Information received from PGE subsequent to completion of this phase of the analysis reduced the loading frequency to 15 cps. This change influences the straining rate dependent temperature shift by 3.5°F . This small change was not incorporated into the analysis.

In the case of the LBLOCA loading, the magnitude of the cyclic load exceeds that of the combined DW and T. It is to be anticipated therefore that the vessel will lift off its support during the negative segment of the LBLOCA loading cycle, thereby extending the vessel first-mode vibration period. Dynamic response time history data for the DW + T + LBLOCA event are, however, not currently available. The loading time for the LBLOCA event has therefore been taken as identical with that of the other dynamic loading events.

The elastic modulus appropriate to the support beam operating temperature (90°F) was obtained from Table I.6 of Ref. 10 as 29.0×10^6 lb/in.² Substituting the above values into Eq. (2) gives an effective crack-tip straining rate for the support beam.

$$\dot{\epsilon}_{\text{beam}} = \frac{2 \times 40 \times 10^3}{0.025 \times 29 \times 10^6} = 0.1103 \text{ in./in./s} ,$$

$$\approx 0.11 \text{ in./in./s} .$$

Substituting for $\dot{\epsilon}$ and σ_y in Eq. (1) gives

$$\Delta T_{\text{beam}} = (150 - 40) \times 0.11^{0.17} = 75.6^\circ\text{F} .$$

Strain rates for the fracture-toughness data of Ref. 7 vary from 10^{-5} in./in./s to 10 in./in./s. Inserting these values into Eq. (1) yields the ΔT values in Table 6.1.

Table 6.1. Temperature adjustment
vs strain rate for A36

Strain rate (in./in./s)	ΔT (°F)	Temperature adjustment (°F)
10^{-5}	15.5	+60.1
10^{-3}	34.0	+41.6
10^{+1}	162.7	-87.1
0.11	75.6	0.0

The strain-rate temperature adjustment in column three of Table 6.1 is applied to the test temperatures for the data of Fig. 6.10 to convert the data to a straining rate appropriate to the vessel-support beam.

Results obtained from the application of the strain-rate correction temperature shift are plotted in Fig. 6.11. All points plotted in the

Fig. 6.11. Fracture-toughness data for A36 after transposition to prototypic reactor vessel support beam straining rate.

figure have been adjusted to the support beam straining rate (0.11 in./in./s).

6.5.2 Data correlation and extrapolation

A best-fit mean curve for the data of Fig. 6.11 was obtained using the Statpad-Curvefit template in conjunction with the Lotus 123 spreadsheet computer program. The program fits 25 different curves to the data and provides a correlation coefficient for each curve. The highest correlation coefficient (0.866) was obtained for a Cauchy curve with the following coefficients:

$$K_{Id} = 1/[1.5 \times 10^{-7} (T - 382.28)^2 + 0.041014] . \quad (3)$$

This curve is shown superposed on the data base in Fig. 6.12.

Primary uses of the mean curve fitted to the data are to provide (1) a basis for extrapolating the data trend to higher temperatures; (2) a base upon which to construct a lower bound curve that in effect defines the design plane-strain, fracture-toughness curve for A36 at the

Fig. 6.12. Best-fit mean curve superposed on transposed A36 fracture-toughness data population.

vessel-support straining rate; and (3) a base for construction of a plane-stress, fracture-toughness K_c curve for use where the plane-strain constraint requirements are not satisfied. It is important therefore that the validity of the mean curve, when extrapolated to higher temperatures, be verified. In particular, it must be shown that the slope ($d K_{Id}/dT$) of the mean curve approaches infinity at a temperature corresponding closely to the limit of applicability of linear elastic fracture mechanics (LEFM).

The highest temperature at which cleavage fracture will occur is defined in Ref. 11 as follows:

$$T \rightarrow T_{\infty} \text{ as } \sigma_y / K_{Id} \rightarrow 0, \quad (4)$$

where

T_{∞} = the upper bound temperature limit for LEFM applicability,

σ_y = dynamic yield stress at temperature T ,

K_{Id} = fracture toughness at temperature T .

An empirical equation for computing the yield stress of a material over the range of temperatures of interest in fracture-toughness evaluations is given in Ref. 12 as follows:

$$\sigma_{y(T,t)} = \sigma_{y(0,0)} + \frac{174,000}{\log(2 \times 10^{10} t)(T + 459)} - 27.4, \quad (5)$$

where

$\sigma_{y(T,t)}$ = dynamic yield stress expressed as a function test temperature and loading time,

$\sigma_{y(0,0)}$ = static room temperature yield stress (40 ksi),

t = loading time (0.025 s),

T = temperature ($^{\circ}\text{F}$).

Yield stress values appropriate to the temperatures for the K_{Id} data points were calculated using Eq. (5). σ_y/K_{Id} values were then calculated and plotted as a function of the test temperature (adjusted for strain rate) in Fig. 6.13. A mean curve was fitted to the data.

Fig. 6.13. Definition of temperature limits for cleavage fracture of A36 at prototypic reactor vessel support beam straining rates.

Extrapolation of this curve to the point where $S_y/K_{Id} = 0$ gave an intersection with the X axis at $T = 77^\circ\text{F}$. This represents the maximum temperature for cleavage fracture based on the mean of the data base. The upper bound limit is obtained in a similar manner as 100°F . Note that these temperature limits apply to unirradiated material.

Extrapolation of the best-fit mean data curve to a temperature of 75°F is shown in Fig. 6.14. The slope of the curve can be seen to increase rapidly at $T = 75^\circ\text{F}$. The calculated slope at $T = 77^\circ\text{F}$ is $1.6 \text{ ksi}/^\circ\text{F}$. The best-fit mean data curve is seen to be consistent with the limits defined in Ref. 11 and is thus considered validated as a basis for constructing the fracture-toughness design curve for A36.

It is important to recognize that the extrapolated K_{Id} curves, while providing a valid base for constructing the final fracture-toughness design curve, are not suitable for direct use in a fracture-toughness evaluation. This limitation results from the fact that the constraint conditions at the crack tip in the material change from plane strain to plane stress as the ratio of fracture toughness to dynamic yield stress increases. The use of the extrapolated curve in deriving the fracture-toughness design curve is discussed further in the following section.

Fig. 6.14. Extrapolation of best-fit, mean fracture-toughness curve for A36, showing compatibility of fitted curve with cleavage-fracture temperature limit.

6.5.3 Plane-strain to plane-stress transition

Irwin has defined the upper bound limit for pure plane-strain behavior at a crack tip in terms of a β parameter as follows:¹³

$$\beta_{Ic} = \frac{1}{B} \left[\frac{K_{Ic}}{\sigma_{ys}} \right]^2 \leq 0.4 , \quad (6)$$

where

B = material thickness,

K_{Ic} = plane-strain fracture toughness,

σ_{ys} = dynamic yield stress.

The data of Fig. 6.9 are also given in Fig. 4.12 of Ref. 13, with $\beta = 0.4$ intercepts superposed on the fracture-toughness curves. These intercepts indicate that the limiting K_{Id} value for plane-strain conditions at the crack tip is $48 \text{ ksi}\cdot\sqrt{\text{in.}}$. Above this value, adjustments must be made for the onset of plane-stress behavior at the crack tip.

The relationship between the plane-strain fracture toughness K_{Ic} and the plane-stress fracture toughness K_c is given in Ref. 13 as follows:

$$K_c^2 = K_{Ic}^2 (1 + 1.4 \times \beta_{Ic}^2) . \quad (7)$$

Equation (7) has been demonstrated to be accurate only for cases in which differences between K_{Ic} and K_c are small. In the absence of plane-stress, fracture-toughness data, however, Eq. (7) was used to predict plane-stress, fracture-toughness behavior.

The plane-stress, fracture-toughness curve is shown superposed on the data base, together with the plane-strain, fracture-toughness curve in Fig. 6.15. Note that the difference between the best-fit plane strain and the estimated plane-stress curves at the $\beta = 0.4$ limit is small ($2 \text{ ksi}\cdot\sqrt{\text{in.}}$). The two mean curves were blended together, starting at the point $K_{Ic} = 48 \text{ ksi}\cdot\sqrt{\text{in.}}$ on the plane-strain, fracture-toughness curve. Note that the K_{Ic} value of $48 \text{ ksi}\cdot\sqrt{\text{in.}}$ corresponds to a strain-rate adjusted temperature of -15°F . The blending curve is shown in Fig. 6.16.

Fig. 6.15. Plane-stress, fracture-toughness curve for A36 derived from best-fit, plain-strain K_{Ic} curve.

Fig. 6.16. Blending of the mean plane-stress and plane-strain fracture-toughness curves for A36.

6.5.4 Fracture-toughness design curves

A lower bound, plane-strain, fracture-toughness curve was constructed by applying a 50°F temperature shift to the plane-strain portion of the mean value curve.* The plane-stress extension of the lower bound curve was then constructed by applying Eq. (7) to the lower bound plane-strain curve for temperatures above -15°F. The resulting curve is shown in Fig. 6.17, superposed on the data set. Note that the lower bound curve passes through the lowest data point.

Requirements for impact testing of materials intended for service in nuclear component supports are given in Subsect. NF of Sect. III of the *ASME Boiler and Pressure Vessel Code*.¹⁴ These requirements were used in a further validity check on the lower bound curve of Fig. 6.17.

Impact testing requirements for materials intended for service in component supports are defined in Paragraph NF-2311 of Ref. 14. Impact

*The 50°F shift is suggested by examining the shift of the ASME lower bound K_{IC} or K_{IR} curve from the mean curves of the corresponding data sets ("Flaw Evaluation Procedures," ASME Sect. XI, EPRI NP-719-SR, August 1978).

Fig. 6.17. Lower bound, plane-strain/plane-stress, fracture-toughness curves for A36 showing compatibility with ASME Code temperature limit for exemption from fracture-toughness testing.

testing is not required when the support service temperature exceeds $NDTT + 30^{\circ}F$. $NDTT$ for A36 plate is given in Table NF-2311(b)-1 as $40^{\circ}F$, giving a lower bound service temperature (for unirradiated material) of $70^{\circ}F$ for exemption from the impact testing requirement. In the following discussion, this temperature is interpreted as a lower bound temperature for design without consideration for brittle fracture.

The $70^{\circ}F$ temperature limit from Ref. 14 is shown superposed on the lower bound, fracture-toughness curve in Fig. 6.17. The lower bound curve increases rapidly as it approaches the Ref. 14 limiting value. Intersection of the lower bound curve with the $70^{\circ}F$ limit occurs at a K_{Id} value of $120 \text{ ksi}\cdot\sqrt{\text{in}}$. Analysis results in Sect. 6.7 (Table 6.17) show critical flaw depths to be increasing rapidly as the critical stress-intensity factor increases by $10 \text{ ksi}\cdot\sqrt{\text{in}}$ from 70 to $80 \text{ ksi}\cdot\sqrt{\text{in}}$. Critical flaw depths at a K_{Id} value of $120 \text{ ksi}\cdot\sqrt{\text{in}}$ would be predicted to be very large and, therefore, compatible with elimination of the requirement for materials impact testing. The lower bound curve of Fig. 6.16 is therefore compatible with the interpretation of the fracture-toughness behavior of unirradiated A36 material incorporated into the materials impact testing requirements of Ref. 14.

The final step in construction of the fracture-toughness design curve was to replot the curve in terms of $T - NDTT$. The $NDTT$ was obtained from Ref. 7 as $28^{\circ}F$. The resulting fracture-toughness design curve is given in Fig. 6.18.

Fig. 6.18. Fracture-toughness design curve for A36 at prototypic reactor vessel straining rates.

6.5.5 A36 NDTT variation

The NDTT for A36 plate used in this analysis was taken from Ref. 7 as 28°F. Heat-to-heat variations in NDTT for a range of materials used in PWR component supports were evaluated in an extensive survey conducted by Sandia National Laboratory.¹⁵ Results quoted in Ref. 15 for carbon-manganese steels, including A36, give the mean NDTT value as 22°F, and a population standard deviation σ as 13°F. The (mean + 1.3 σ) value for NDTT, corresponding to a 90% confidence limit, is given as 39°F. An increase of 11°F in NDTT must therefore be applied to the results given in this report to obtain critical flaw depths corresponding to a 90% confidence limit on NDTT.

A carpet plot giving critical flaw depths as a function of (1) T - NDTT and (2) support loading is given in Fig. 6.49 (Sect. 6.7) for the beam flange grout-hole location. This plot can be used to rapidly assess the impact of applying the 90°F confidence limit temperature shift (11°F) on the calculated critical flaw depths.

6.5.6 Supporting analysis

A summary of analyses performed to construct the A36 fracture-toughness, lower bound curves is provided in Tables 6.2-6.5.

6.5.7 Reactor support/bridge structure comparison

A36 is widely used in bridge construction where it has performed satisfactorily. It is of interest therefore to compare the service conditions appropriate to bridge application with those encountered in the reactor vessel-support beam application. In particular, the effect of the differing dynamic response characteristics of the two structural systems will be considered here.

Bridges are typically relatively flexible structures with low natural frequencies. Peak straining rates in bridge structures during response to impulsive loading are limited by their low natural frequency to relatively low values. Strain rates induced by the application of live loads are likewise limited to relatively low values by the bridge-span traverse time of heavily loaded vehicles. Rolfe and Barsom¹³ place the upper bound, crack-tip straining rate for bridge structures under dynamic loading conditions at 0.001 in./in./s. This straining rate is ~2 orders of magnitude slower than that encountered in the vessel-support beam application. The temperature-adjustment results of Table 6.1 show that this straining rate difference produces a temperature shift of ~40°F. Design stress-intensity-factor values at a given temperature for the support beam application are thus lower than the corresponding values for a bridge application.

Table 6.2. Construction of the A36 plane strain fracture-toughness curves for reactor support loading rates

Test Temperature	Test Strain Rate	Fracture Toughness	Straining Rate Temp. Adjustment Increment	Temperature Adjusted for Straining Rate	Sy/K1c	Calculated Best Fit Mean Curve .11in/in/s Strain Rate	Calculated Lower Bound Curve .11in/in/s Strain Rate	T-MDT	Yield Stress
Deg. F.	in/in/sec	KSI*in ^{1/2}	Deg. F.	Deg. F.			(Mean-50)	Deg. F.	
				1.000E+02			7.702E+01	7.200E+01	
				9.500E+01			7.337E+01	6.700E+01	
				9.000E+01			7.010E+01	6.200E+01	
				8.500E+01			6.713E+01	5.700E+01	
				8.000E+01			6.444E+01	5.200E+01	
				7.500E+01			6.199E+01	4.700E+01	
				7.000E+01			5.974E+01	4.200E+01	
				6.500E+01			5.767E+01	3.700E+01	
				6.000E+01			5.577E+01	3.200E+01	
				5.500E+01			5.401E+01	2.700E+01	
				5.000E+01		7.702E+01	5.237E+01	2.200E+01	
				4.500E+01		7.337E+01	5.086E+01	1.700E+01	
				4.000E+01		7.010E+01	4.944E+01	1.200E+01	
				3.500E+01		6.713E+01	4.812E+01	7.000E+00	
				3.000E+01		6.444E+01	4.689E+01	2.000E+00	
				2.500E+01		6.199E+01	4.573E+01	-3.000E+00	
				2.000E+01		5.974E+01	4.465E+01	-8.000E+00	
				1.500E+01		5.767E+01	4.362E+01	-1.300E+01	
				1.000E+01		5.577E+01	4.266E+01	-1.800E+01	
				5.000E+00		5.401E+01	4.175E+01	-2.300E+01	
				0.000E+00		5.237E+01	4.090E+01	-2.800E+01	
				-1.000E+01		4.944E+01	3.932E+01	-3.800E+01	
				-1.500E+01		4.812E+01	3.859E+01	-4.300E+01	
				-2.000E+01		4.689E+01	3.790E+01	-4.800E+01	
				-2.500E+01		4.573E+01	3.724E+01	-5.300E+01	
				-3.000E+01		4.465E+01	3.662E+01	-5.800E+01	
-7.500E+01	1.000E-03	4.150E+01	4.160E+01	-3.340E+01	1.436E+00	4.394E+01	3.621E+01	-6.140E+01	5.960E+01
-1.080E+02	1.000E-05	4.580E+01	6.010E+01	-4.790E+01	1.337E+00	4.125E+01	3.462E+01	-7.590E+01	6.126E+01
3.800E+01	1.000E+01	3.900E+01	-8.710E+01	-4.910E+01	1.574E+00	4.105E+01	3.450E+01	-7.710E+01	6.140E+01
-9.300E+01	1.000E-03	3.700E+01	4.160E+01	-5.140E+01	1.667E+00	4.066E+01	3.427E+01	-7.940E+01	6.167E+01
-9.500E+01	1.000E-03	3.900E+01	4.160E+01	-5.340E+01	1.588E+00	4.034E+01	3.407E+01	-8.140E+01	6.192E+01
2.600E+01	1.000E+01	4.500E+01	-8.710E+01	-6.110E+01	1.397E+00	3.915E+01	3.335E+01	-8.910E+01	6.287E+01
-1.240E+02	1.000E-05	4.050E+01	6.010E+01	-6.390E+01	1.561E+00	3.875E+01	3.310E+01	-9.190E+01	6.323E+01
-1.280E+02	1.000E-05	4.450E+01	6.010E+01	-6.790E+01	1.432E+00	3.818E+01	3.276E+01	-9.590E+01	6.374E+01
1.400E+01	1.000E+01	3.700E+01	-8.710E+01	-7.310E+01	1.741E+00	3.749E+01	3.232E+01	-1.011E+02	6.443E+01
-1.330E+02	1.000E-03	3.300E+01	4.160E+01	-9.140E+01	2.031E+00	3.531E+01	3.095E+01	-1.194E+02	6.701E+01
-1.000E+01	1.000E+01	3.850E+01	-8.710E+01	-9.710E+01	1.763E+00	3.470E+01	3.056E+01	-1.251E+02	6.787E+01
-1.400E+02	1.000E-03	3.050E+01	4.160E+01	-9.840E+01	2.232E+00	3.457E+01	3.048E+01	-1.264E+02	6.807E+01
-1.600E+02	1.000E-05	3.430E+01	6.010E+01	-9.990E+01	1.991E+00	3.442E+01	3.038E+01	-1.279E+02	6.830E+01
-1.650E+02	1.000E-05	3.350E+01	6.010E+01	-1.049E+02	2.062E+00	3.393E+01	3.007E+01	-1.329E+02	6.909E+01
-1.900E+01	1.000E+01	3.450E+01	-8.710E+01	-1.061E+02	2.008E+00	3.381E+01	2.999E+01	-1.341E+02	6.928E+01
-1.600E+02	1.000E-03	3.050E+01	4.160E+01	-1.184E+02	2.339E+00	3.271E+01	2.928E+01	-1.464E+02	7.133E+01
-6.000E+01	1.000E+01	3.300E+01	-8.710E+01	-1.471E+02	2.325E+00	3.056E+01	2.788E+01	-1.751E+02	7.673E+01
-1.900E+02	1.000E-03	2.850E+01	4.160E+01	-1.484E+02	2.702E+00	3.048E+01	2.782E+01	-1.764E+02	7.700E+01
-2.280E+02	1.000E-05	2.960E+01	6.010E+01	-1.679E+02	2.747E+00	2.931E+01	2.706E+01	-1.959E+02	8.131E+01
-1.100E+02	1.000E+01	2.950E+01	-8.710E+01	-1.971E+02	3.016E+00	2.788E+01	2.613E+01	-2.251E+02	8.897E+01
-1.180E+02	1.000E+01	3.050E+01	-8.710E+01	-2.051E+02	2.996E+00	2.754E+01	2.591E+01	-2.331E+02	9.138E+01

Table 6.3

* CURVEFIT * CURVE FITTING		No. of data rows			24
CURVE #		a	b	c	RR(adj'd)
1)	bX		-0.16314		n/a
2)	a+bX	41.76978	0.057591		0.6592567
3)	bX+cX^2		-0.42282	-0.00100	n/a
4)	a+bX+cX^2	47.51319	0.144399	0.000219	0.7890883
5)	1/(a+bX)	0.022956	-0.00005		0.7887316
6)	X/(aX+b)	0.037985	0.690076		0.6785572
7)	a+b/X	25.42168	-781.488		0.6837600
8)	a+bX+c/X	32.91161	0.030140	-464.582	0.7452817
9)	a+b/X+c/X^2	19.24268	-1933.53	-40096.6	0.8095685
MAX RR CURVES 1 to 9 =					0.8095685

CURVE #		a	b	c	RR(adj'd)
10)	aX^b	ERR	ERR		0
11)	ab^(X)	42.42251	1.001770		0.7277982
12)	ab^(1/X)	25.97992	1.0E-10		0.6887448
13)	aX^(bX)	ERR	ERR		0
14)	aX^(b/X)	ERR	ERR		0
15)	aX/b^X	ERR	ERR		n/a
16)	ab^X*X^c	ERR	ERR	ERR	0
17)	ab^(1/X)*X^c	ERR	ERR	ERR	0
18)	1/[a(X+b)^2 + c]	-0.00000	382.2796	0.041014	0.8662148
19)	a+b*lnX	ERR	ERR		0
20)	1/(a+b*lnX)	ERR	ERR		0
21)	ae^(bX)	42.42251	0.001768		0.7277982
22)	ae^(b/X)	25.97992	-22.9892		0.6887448
23)	ae^[(X-b)^2]/c	24.22951	-350.864	173051.4	0.8327229
24)	ae^([(b-lnX)^2]/c)	ERR	ERR	ERR	0
25)	a[(X/b)^c]e^(X/b)	ERR	ERR	ERR	0
MAX RR CURVES 10 to 25 =					0.8662148

DESC STATS X Y
TOTAL -3043.4 827.2 * TITLE * A36 FRACTURE TOUGHNESS DATA
MEAN -126.8083 34.466666 TITLE X ADJUSTED TEMPERATURE (Deg.F)
n 24 24 TITLE Y FRACTURE TOUGHNESS : K1c
SD 90.879939 6.3749282
MAX -33.4 45.8
MIN -412.1 24.5
FITTED CURVE NUMBER = 18

DATA BELOW	X	Y	ESTIMATED Y	RESIDUAL
ROW			Y = 1/[a(X+b)^2 + c]	
1	-33.4	41.5	44.06757	2.567574
2	-47.9	45.8	41.35012	-4.44987
3	-49.1	39	41.14497	2.144970
4	-51.4	37	40.75941	3.759410
5	-53.4	39	40.43208	1.432083
6	-61.1	45	39.23678	-5.76321
7	-63.9	40.5	38.82614	-1.67385
8	-67.9	44.5	38.26018	-6.23981
9	-73.1	37	37.55880	0.558807
10	-91.4	33	35.36336	2.363361
11	-97.1	38.5	34.75587	-3.74412
12	-98.4	30.5	34.62187	4.121879
13	-99.9	34.3	34.46929	0.169298
14	-104.9	33.5	33.97591	0.475911
15	-106.1	34.5	33.86087	-0.63912
16	-118.4	30.5	32.75197	2.251972
17	-147.1	33	30.59179	-2.40820
18	-148.4	28.5	30.50613	2.006138
19	-167.9	29.6	29.32891	-0.27108
20	-197.1	29.5	27.89224	-1.60775
21	-205.1	27.5	27.55685	0.056854
22	-264.9	25.5	25.68048	0.180482
23	-283.4	25	25.28937	0.289371
24	-412.1	24.5	24.46173	-0.03826

Table 6.4. Plane strain-plane stress transition (Ref. 13, page 114)

Plate Thickness = 2.500E+00

Temperature Adjusted for Straining Rate	Calculated Plane Strain Mean Curve .11in/in/s Strain Rate	Calculated Plane Strain Lower Bound .11in/in/s Strain Rate (Mean-50)	Dynamic Yield Stress	BETAic	Calculated Plane Stress Mean Curve .11in/in/s Strain Rate	Calculated Plane Stress Lower Bound .11in/in/s Strain Rate (Mean-50)
Deg. F.						
1.000E+02	1.633E+02	7.702E+01	4.838E+01	4.555E+00		
9.500E+01	1.461E+02	7.337E+01	4.871E+01	3.599E+00		
9.000E+01	1.323E+02	7.010E+01	4.903E+01	2.913E+00		
8.500E+01	1.210E+02	6.713E+01	4.937E+01	2.405E+00		
8.000E+01	1.116E+02	6.444E+01	4.971E+01	2.017E+00		
7.500E+01	1.036E+02	6.199E+01	5.006E+01	1.715E+00		
7.000E+01	9.680E+01	5.974E+01	5.041E+01	1.475E+00	1.947E+02	1.202E+02
6.500E+01	9.087E+01	5.767E+01	5.077E+01	1.281E+00	1.650E+02	1.047E+02
6.000E+01	8.567E+01	5.577E+01	5.114E+01	1.123E+00	1.424E+02	9.272E+01
5.500E+01	8.109E+01	5.401E+01	5.152E+01	9.911E-01	1.250E+02	8.323E+01
5.000E+01	7.702E+01	5.237E+01	5.190E+01	8.809E-01	1.112E+02	7.565E+01
4.500E+01	7.337E+01	5.086E+01	5.229E+01	7.877E-01	1.003E+02	6.952E+01
4.000E+01	7.010E+01	4.944E+01	5.268E+01	7.081E-01	9.145E+01	6.450E+01
3.500E+01	6.713E+01	4.812E+01	5.309E+01	6.396E-01	8.419E+01	6.035E+01
3.000E+01	6.444E+01	4.689E+01	5.350E+01	5.803E-01	7.817E+01	5.688E+01
2.500E+01	6.199E+01	4.573E+01	5.393E+01	5.285E-01	7.311E+01	5.394E+01
2.000E+01	5.974E+01	4.465E+01	5.436E+01	4.831E-01	6.881E+01	5.142E+01
1.500E+01	5.767E+01	4.362E+01	5.480E+01	4.430E-01	6.512E+01	4.925E+01
1.000E+01	5.577E+01	4.266E+01	5.525E+01	4.075E-01	6.191E+01	4.736E+01
5.000E+00	5.401E+01	4.175E+01	5.571E+01	3.759E-01	5.911E+01	4.570E+01
0.000E+00	5.237E+01	4.090E+01	5.618E+01	3.477E-01	5.663E+01	4.422E+01
-1.000E+01	4.944E+01	3.932E+01	5.715E+01	2.994E-01	5.245E+01	4.171E+01
-1.500E+01	4.812E+01	3.859E+01	5.765E+01	2.787E-01	5.067E+01	4.063E+01
-2.000E+01	4.689E+01	3.790E+01	5.816E+01	2.600E-01	4.906E+01	3.965E+01
-2.500E+01	4.573E+01	3.724E+01	5.869E+01	2.429E-01	4.758E+01	3.875E+01
-3.000E+01	4.465E+01	3.662E+01	5.923E+01	2.273E-01	4.623E+01	3.792E+01
-3.340E+01	4.394E+01	3.621E+01	5.960E+01	2.175E-01	4.537E+01	3.739E+01
-4.790E+01	4.125E+01	3.462E+01	6.126E+01	1.814E-01	4.219E+01	3.541E+01
-4.910E+01	4.105E+01	3.450E+01	6.140E+01	1.788E-01	4.195E+01	3.526E+01
-5.140E+01	4.066E+01	3.427E+01	6.167E+01	1.739E-01	4.152E+01	3.499E+01
-5.340E+01	4.034E+01	3.407E+01	6.192E+01	1.698E-01	4.115E+01	3.475E+01
-6.110E+01	3.915E+01	3.335E+01	6.287E+01	1.551E-01	3.981E+01	3.391E+01
-6.390E+01	3.875E+01	3.310E+01	6.323E+01	1.502E-01	3.935E+01	3.362E+01
-6.790E+01	3.818E+01	3.276E+01	6.374E+01	1.435E-01	3.873E+01	3.322E+01
-7.310E+01	3.749E+01	3.232E+01	6.443E+01	1.354E-01	3.797E+01	3.274E+01
-9.140E+01	3.531E+01	3.095E+01	6.701E+01	1.110E-01	3.561E+01	3.122E+01
-9.710E+01	3.470E+01	3.056E+01	6.787E+01	1.046E-01	3.497E+01	3.080E+01
-9.840E+01	3.457E+01	3.048E+01	6.807E+01	1.032E-01	3.483E+01	3.071E+01
-9.990E+01	3.442E+01	3.038E+01	6.830E+01	1.016E-01	3.467E+01	3.060E+01
-1.049E+02	3.393E+01	3.007E+01	6.909E+01	9.647E-02	3.415E+01	3.026E+01
-1.061E+02	3.381E+01	2.999E+01	6.928E+01	9.529E-02	3.403E+01	3.018E+01
-1.184E+02	3.271E+01	2.928E+01	7.133E+01	8.414E-02	3.287E+01	2.943E+01
-1.471E+02	3.056E+01	2.788E+01	7.673E+01	6.347E-02	3.065E+01	2.796E+01
-1.484E+02	3.048E+01	2.782E+01	7.700E+01	6.268E-02	3.056E+01	2.790E+01
-1.679E+02	2.931E+01	2.706E+01	8.131E+01	5.197E-02	2.936E+01	2.711E+01

Table 6.5. Composite plane strain-plane stress fracture-toughness curve

Temperature Adjusted for Straining Rate	Calculated Plane Strain Mean Curve .11in/in/s Strain Rate	Calculated Plane Stress Mean Curve .11in/in/s Strain Rate	Composite Mean Curve .11in/in/s Strain Rate	Composite Lower Bound Curve .11in/in/s Strain Rate	T-NDT
Deg. F.					Deg. F.
1.000E+02					7.200E+01
9.500E+01					6.700E+01
9.000E+01					6.200E+01
8.500E+01					5.700E+01
8.000E+01					5.200E+01
7.500E+01					4.700E+01
7.000E+01	9.680E+01				4.200E+01
6.500E+01	9.087E+01				3.700E+01
6.000E+01	8.567E+01			9.272E+01	3.200E+01
5.500E+01	8.109E+01			8.323E+01	2.700E+01
5.000E+01	7.702E+01			7.565E+01	2.200E+01
4.500E+01	7.337E+01	1.000E+02	1.000E+02	6.952E+01	1.700E+01
4.000E+01	7.010E+01	9.145E+01	9.145E+01	6.450E+01	1.200E+01
3.500E+01	6.713E+01	8.419E+01	8.419E+01	6.035E+01	7.000E+00
3.000E+01	6.444E+01	7.817E+01	7.817E+01	5.688E+01	2.000E+00
2.500E+01	6.199E+01	7.311E+01	7.282E+01	5.371E+01	-3.000E+00
2.000E+01	5.974E+01	6.851E+01	6.824E+01	5.097E+01	-8.000E+00
1.500E+01	5.767E+01	6.515E+01	6.427E+01	4.857E+01	-1.300E+01
1.000E+01	5.577E+01	6.191E+01	6.078E+01	4.646E+01	-1.800E+01
5.000E+00	5.401E+01	5.911E+01	5.769E+01	4.456E+01	-2.300E+01
0.000E+00	5.237E+01	5.653E+01	5.493E+01	4.286E+01	-2.800E+01
-1.000E+01	4.944E+01	5.245E+01	5.019E+01	3.990E+01	-3.800E+01
-1.500E+01	4.812E+01	5.067E+01	4.812E+01	3.859E+01	-4.300E+01
-2.000E+01	4.689E+01	4.905E+01	4.689E+01	3.790E+01	-4.800E+01
-2.500E+01	4.573E+01	4.753E+01	4.573E+01	3.724E+01	-5.300E+01
-3.000E+01	4.465E+01	4.623E+01	4.465E+01	3.662E+01	-5.800E+01
-3.340E+01	4.394E+01	4.537E+01	4.394E+01	3.621E+01	-6.140E+01
-4.790E+01	4.125E+01	4.215E+01	4.125E+01	3.462E+01	-7.590E+01
-4.910E+01	4.105E+01	4.195E+01	4.105E+01	3.450E+01	-7.710E+01
-5.140E+01	4.066E+01	4.152E+01	4.066E+01	3.427E+01	-7.940E+01
-5.340E+01	4.034E+01	4.115E+01	4.034E+01	3.407E+01	-8.140E+01
-6.110E+01	3.915E+01	3.981E+01	3.915E+01	3.335E+01	-8.910E+01
-6.390E+01	3.875E+01	3.935E+01	3.875E+01	3.310E+01	-9.190E+01
-6.790E+01	3.818E+01	3.873E+01	3.818E+01	3.276E+01	-9.590E+01
-7.310E+01	3.749E+01	3.797E+01	3.749E+01	3.232E+01	-1.011E+02
-9.140E+01	3.531E+01	3.561E+01	3.531E+01	3.095E+01	-1.194E+02
-9.710E+01	3.470E+01	3.497E+01	3.470E+01	3.056E+01	-1.251E+02
-9.840E+01	3.457E+01	3.483E+01	3.457E+01	3.048E+01	-1.264E+02
-9.990E+01	3.442E+01	3.467E+01	3.442E+01	3.038E+01	-1.279E+02
-1.049E+02	3.393E+01	3.415E+01	3.393E+01	3.007E+01	-1.329E+02
-1.061E+02	3.381E+01	3.403E+01	3.381E+01	2.999E+01	-1.341E+02
-1.184E+02	3.271E+01	3.287E+01	3.271E+01	2.928E+01	-1.464E+02
-1.471E+02	3.056E+01	3.065E+01	3.056E+01	2.788E+01	-1.751E+02
-1.484E+02	3.048E+01	3.056E+01	3.048E+01	2.782E+01	-1.764E+02
-1.679E+02	2.931E+01	2.936E+01	2.931E+01	2.706E+01	-1.959E+02
-1.971E+02	2.788E+01	2.791E+01	2.788E+01	2.613E+01	-2.251E+02
-2.051E+02	2.754E+01	2.757E+01	2.754E+01	2.591E+01	-2.331E+02
-2.649E+02	2.568E+01	2.568E+01	2.568E+01	2.479E+01	-2.929E+02

6.5.8 Fracture-toughness data evolution

Recently published analyses of shallow- and deep-crack A36 CTOD fracture-toughness specimens indicate that higher fracture toughness can be generated in specimens with small crack depths.¹⁶ This development is noted as a pointer to considerations for possible inclusion in any further evaluation of the support beams. The results of Ref. 16 are not incorporated into the current analysis.

6.6 Radiation-Induced NDTT Shift

NDTT shift (Δ NDTT) values were calculated for three governing locations on the support beams for both the current condition existing at Trojan and for 32 EFPY. The steps in the analysis were as follows:

1. Determine the core midplane dpa-rate profile into the reactor cavity wall for the Trojan initial core configuration. Select dpa rates for the three governing locations.
2. Catalog the Trojan operating history to date and determine the projected operating condition in terms of the ratio R of the reactor cavity dpa rate to the cavity dpa rate associated with the first core. Calculate the time-averaged, dpa-rate ratios for both prior operations and projected lifetime operations.
3. Using the dpa-rate ratios from step (2) in conjunction with the cycle 1 dpa rates from step (1), calculate effective dpa rates and total dpas for both the current condition and the projected end of life condition.
4. Using the dpa and dpa-rate data from step (3), enter Fig. 3.4 to obtain the Δ NDTT for each of the three governing locations on the beam (see Fig. 6.19).

Core midplane dpa data were obtained from Westinghouse¹⁷ and are plotted in Fig. 6.20. Note that the data points plotted in Fig. 6.20 are the result of calculations normalized to a measured dpa rate for Diablo Canyon Unit 2. The dpa rates for the points of interest on the support beams were as follows:

<u>Beam location</u>	<u>dpa/s</u>
Cavity inner surface interface	2.79×10^{-12}
Node 4: Maximum bending moment	2.65×10^{-13}
Flange hole centerline	1.09×10^{-13}

Trojan operating history data and planned future operating conditions were obtained from Ref. 18. These data are reproduced in Table 6.6 and indicate the following dpa ratios:

Operations to date $R = 7.593/7.48 = 1.015$

Future operation = 0.65

Fig. 6.19. Radiation-induced NDTT shifts at critical locations on Trojan reactor vessel support beams.

Fig. 6.20. dpa rate within Trojan reactor cavity wall at reactor vessel support beam elevation.

Table 6.6. Trojan operating history

Reactor cycle	EFPY	Three-assembly average power ratio	Weighted EFPY
1	1.13	1	1.13
2	0.78	1.26	0.9828
3	0.69	1.23	0.8487
4	0.59	1.25	0.7375
5	0.37	1.26	0.4662
6	0.68	1.12	0.7616
7	0.51	0.75	0.3825
8	0.69	0.75	0.5175
9	0.74	0.88	0.6512
10	0.5	0.867	0.435
11	0.8	0.85	0.68
12 Onward		0.65	
Totals	7.48		7.593

Results from the analysis of steps (3) and (4) are summarized in Table 6.7. At the present time Trojan has operated for <25% of its presently licensed life (32 EFPY). The maximum dpa in that portion of the support beam that projects into the reactor cavity has reached a level where a Δ NDTT of 75°F is predicted. Δ NDTT at this location is predicted to increase to 180°F by 32 EFPY. Corresponding values of

Table 6.7. NDTT shift (Δ NDTT)

Beam location ^a	Cavity inner surface interface	Node 4: Maximum bending moment location	Beam flange hole center-line
Cycle 1 dpa rate (dpa/s)	2.790000E-12	2.650000E-13	1.090000E-13
Average dpa ratio to date	1.015	1.015	1.015
EFPY to date	7.48	7.48	7.48
Total dpa to date	0.0006680878	0.0000634564	0.0000261009
dpa rate to date	2.831850E-12	2.689750E-13	1.106350E-13
NDTT shift to date (°F)	75	38	30
Remaining lifetime (EFPY)	24.52	24.52	24.52
Planned dpa ratio	0.65	0.65	0.65
Additional dpa	0.0014024898	0.0001332114	0.0000547926
32-EFPY dpa	0.0020705776	0.0001966678	0.0000808935
Average lifetime dpa rate	2.051539E-12	1.948595E-13	8.014974E-14
Δ NDTT at 32 EFPY (°F)	180	96	75

ΔNDTT at the beam flange grout-hole centerline are 30°F at the present time and 75°F at 32 EFPY.

T – NDTT values were calculated for both the current condition and 32 EFPY for a range of support beam operating temperatures. The support beam operating temperature has been set at 90°F (Ref. 2), but a range of support beam operating temperatures was included in the analyses to provide some insight into the impact of potential refinements in the definition of this parameter.

The T – NDTT values were superposed on the fracture-toughness design curve of Fig. 6.18 to obtain fracture-toughness design values for each of the critical locations on the support beam for initial, current, and 32-EFPY conditions. The resulting fracture-toughness design values are summarized in Table 6.8.

Table 6.8. Design dynamic stress-intensity factors

Location On Vessel Support Beam	Equivalent Full Power Years Consumed	Irradiation Induced NDT Shift	Design Dynamic Fracture Toughness	Design Dynamic Fracture Toughness	Design Dynamic Fracture Toughness	Design Dynamic Fracture Toughness
(a) T – NDT						
Support beam temperature(DegF)			9.000E+01	1.000E+02	1.100E+02	1.200E+02
Cavity liner interface	0.000E+00	0.000E+00	6.200E+01	7.200E+01	8.200E+01	9.200E+01
Cavity liner interface	7.480E+00	7.400E+01	-1.200E+01	-2.000E+00	8.000E+00	1.800E+01
Cavity liner interface	3.200E+01	1.800E+02	-1.180E+02	-1.080E+02	-9.800E+01	-8.800E+01
Node 4 : Max. Bending Moment	0.000E+00	0.000E+00	6.200E+01	7.200E+01	8.200E+01	9.200E+01
Node 4 : Max. Bending Moment	7.480E+00	3.800E+01	2.400E+01	3.400E+01	4.400E+01	5.400E+01
Node 4 : Max. Bending Moment	3.200E+01	9.600E+01	-3.400E+01	-2.400E+01	-1.400E+01	-4.000E+00
Beam flange hole	0.000E+00	0.000E+00	6.200E+01	7.200E+01	8.200E+01	9.200E+01
Beam flange hole	7.480E+00	3.000E+01	3.200E+01	4.200E+01	5.200E+01	6.200E+01
Beam flange hole	3.200E+01	7.500E+01	-1.300E+01	-3.000E+00	7.000E+00	1.700E+01
(b) Design fracture toughness						
Cavity liner interface	0.000E+00	0.000E+00	2.500E+02	>250	>250	>250
Cavity liner interface	7.480E+00	7.400E+01	4.900E+01	5.434E+01	6.118E+01	7.074E+01
Cavity liner interface	3.200E+01	1.800E+02	3.100E+01	3.180E+01	3.250E+01	3.344E+01
Node 4 : Max. Bending Moment	0.000E+00	0.000E+00	2.500E+02	>250	>250	>250
Node 4 : Max. Bending Moment	7.480E+00	3.800E+01	7.868E+01	9.750E+01	1.282E+02	1.810E+02
Node 4 : Max. Bending Moment	3.200E+01	9.600E+01	4.108E+01	4.422E+01	4.815E+01	5.316E+01
Beam flange hole	0.000E+00	0.000E+00	2.516E+02	>250	>250	>250
Beam flange hole	7.480E+00	3.000E+01	9.272E+01	1.202E+02	1.667E+02	2.516E+02
Beam flange hole	3.200E+01	7.500E+01	4.857E+01	5.371E+01	6.035E+01	6.952E+01

In the case of the beam flange grout hole, additional analyses were performed to define the design fracture toughness as a function of time between the current time (7.48 EFPY) and 32 EFPY. Results from this additional analysis are summarized in Fig. 6.21. Supporting calculations for Fig. 6.21 are summarized in Table 6.9. Note that while

Fig. 6.21. Predicted lifetime variation in fracture toughness of A36 at reactor vessel support beam grout-hole location.

Table 6.9. Lifetime variation in T - NDTT and material fracture-toughness, beam-flange-hole location

BEAM FLANGE HOLE RADIAL FLAWS

E.F.P.Y.	7.48E+00	1.00E+01	1.50E+01	2.00E+01	2.50E+01	3.20E+01
Total D.P.A.	2.61E-05	3.17E-05	4.29E-05	5.41E-05	6.53E-05	8.09E-05
	1.11E-13	1.01E-13	9.07E-14	8.57E-14	8.28E-14	8.02E-14
RT NDT Shift	3.00E+01	3.60E+01	4.60E+01	5.50E+01	6.40E+01	7.50E+01
Design fracture toughness	9.27E+01	8.17E+01	7.08E+01	6.04E+01	5.43E+01	4.86E+01

the design fracture toughness is a nonlinear function of operating time, $\Delta NDTT$ varies with time in a roughly linear manner between 7.48 and 32.0 EFPY. An increase in the beam operating temperature can thus be used to offset additions to the reactor operating period using the following relationship:

$$\Delta(EFPY)_{EFFECTIVE} = \Delta(EFPY)_{ACTUAL} - 0.54 (T - 90) , \quad (8)$$

where T is the operating temperature at the location of concern on the support beam (°F).

6.7 Critical-Flaw-Depth Analysis

Stress-intensity factors were calculated using the methods and data of Article A-3000 of Ref. 19. Lotus 123 spreadsheet templates were prepared to accept input from (1) the unit loading beam bending analysis, (2) the load case definitions, (3) the flaw shape parameter Q, and (4) the flaw membrane and bending stress correction factors M_m and M_b . The spreadsheets then calculated the flaw-tip, stress-intensity factor K_I for a range of flaw depths (a) and flaw aspect ratios (a/l).

Note that the flaw-tip, stress-intensity factors are a nonlinear function of the applied loading. The analysis results cannot therefore be directly factored for application to other loading conditions. The analyses were, however, performed for four different peak loading conditions. A plot of results for these loading conditions provides a means of estimating the flaw-tip, stress-intensity factor for intermediate loading conditions.

Results from the flaw-tip, stress-intensity analyses were plotted as a function of flaw depth for each of the flaw aspect ratios evaluated. Fracture-toughness values determined in Sect. 6.6 (see Table 6.8) were then superposed on the K_I plots. Critical flaw depths were obtained from the intersection of the K_I and fracture-toughness curves. Finally, the critical flaw depths calculated for each of the four loading conditions evaluated were combined in a single plot, giving critical flaw depth as a function of the maximum vessel-support load.

6.7.1 Cavity linear interface

A sample of the spreadsheets used to calculate stress-intensity factors at this location is given in Table 6.10. Engineers bending theory was used to calculate a linear distribution of bending stress in the overall beam section. The membrane and bending elements of stress in the upper flange were then calculated. Using these results, flaw-tip, stress-intensity factors were calculated for each of the flaw depths using the equation of Ref. 19, which is reproduced below as

$$K_I = \sigma_m M_m \sqrt{\pi} \sqrt{a/Q} + \sigma_b M_b \sqrt{\pi} \sqrt{a/Q} , \quad (9)$$

Table 6.10

TROJAN : SUPPORT BEAM CRITICAL FLAW SIZE ANALYSIS

Stress location :	!Interface with the cavity liner					
Flaw type	!Elliptical surface flaw					
Flaw aspect ratio a/l	: 0.100					
Plate thickness in.	: 2.500					
Stresses in beam flange without flaw :	!Unit load	!Load case	!Load case	!Max. surface	!Flange mean	!Flange bend. :
	!(200 Kip) BM	!maximum load	!maximum BM	!stress	!stress	!stress :
Faulted (2) = DW + T + SB:LOCA	: 969000.000	: 1558000.000	: 7548510.000	: 12380.470	: 10660.960	: 1719.510 :
Faulted (3) = DW + T + LB:LOCA	: 969000.000	: 2443000.000	: 11836335.000	: 19413.022	: 16716.769	: 2696.253 :
Flaw shape parameter = Q	Sy =	40000.000	!Faulted(2)	(Sm + Sb)/Sy =	0.310	Q = 1.090
			!Faulted(3)	(Sm + Sb)/Sy =	0.485	Q = 1.060
Flaw depth "a" in.	: 0.050	: 0.100	: 0.200	: 0.300	: 0.600	: 1.250 :
Flaw depth to flange thickness ratio	: 0.020	: 0.040	: 0.080	: 0.120	: 0.240	: 0.500 :
Membrane correction factor Mm	: 1.100	: 1.100	: 1.120	: 1.140	: 1.250	: 1.810 :
Bending correction factor Mb	: 1.040	: 1.020	: 0.980	: 0.940	: 0.870	: 0.890 :
Faulted condition (2)						
(a) = Sm*Mm*2SQRT(PI)*2SQRT(a/Q)	: 4451.800	: 6295.797	: 9065.485	: 11301.172	: 17524.420	: 36626.177 :
(b) = Sb*Mb*2SQRT(PI)*2SQRT(a/Q)	: 678.867	: 941.600	: 1279.403	: 1502.986	: 1967.257	: 2904.767 :
KI = (a) + (b)	: 5130.667	: 7237.397	: 10344.888	: 12804.158	: 19491.677	: 39530.944 :
Faulted condition (3)						
(a) = Sm*Mm*2SQRT(PI)*2SQRT(a/Q)	: 7078.676	: 10010.759	: 14414.758	: 17969.658	: 27865.059	: 58238.196 :
(b) = Sb*Mb*2SQRT(PI)*2SQRT(a/Q)	: 1079.446	: 1497.210	: 2034.341	: 2389.853	: 3129.078	: 4618.784 :
KI = (a) + (b)	: 8158.122	: 11507.970	: 16449.099	: 20359.511	: 30993.137	: 62856.981 :

where

σ_m, σ_b = membrane and bending stresses (psi) in accordance with A-3200;

a = minor half-diameter (in.) of embedded flaw, flaw depth for surface flaw;

Q = flaw shape parameter as determined from Fig. A-3300-1 using $(\sigma_m + \sigma_b)/\sigma_{ys}$ and the flaw geometry;

M_m = correction factor for membrane stress (see Fig. A-3300-2 for subsurface flaws and Fig. A-3300-3 for surface flaws);

M_b = correction factor for bending stress (see Fig. A-3300-4 for subsurface flaws and Fig. A-3300-5 for surface flaws).

Considerations of the effectiveness of the inner section of the concrete foundation in providing support to the beam do not influence either the distribution of bending stresses or the calculation of flaw-tip, stress-intensity factors at the cavity liner interface location.

A summary of flaw-tip, stress-intensity factors calculated for the support beam flange at the reactor cavity liner interface is given in Table 6.11. The analysis sheets from which these data were taken are provided in Appendix 2.

Plots of the flaw-tip, stress-intensity factors for each of the load combinations considered are given in Figs. 6.22-6.25. The 32-EFPY K_{I_d} value derived for this location on the beam is 31 ksi $\cdot\sqrt{\text{in.}}$. Critical flaw depths corresponding to 7.48 and 32 EFPY of operation are shown plotted in Figs. 6.26 and 6.27. The critical depth under SBLOCA loading for a uniform flaw extending across the entire 16-in. width of the beam flange ($a/l = 0$) ranges from a maximum of 0.89 in. at the current time (7.48 EFPY) to a minimum of 0.68 in. at 32 EFPY. The $a/l = 0$ flaw is common to all locations evaluated and therefore provides a useful measure of the relative severity of the brittle fracture potential at each location.

Note that the flaw tolerance at the cavity liner interface increases rapidly as the flaw aspect ratio increases above zero. Critical flaw depths were not calculated for cases in which $a > t/2$.

A non-heat-treated fillet weld exists at the interface between the support beam flange and the reactor cavity liner. Residual stresses from the weld have not been incorporated in the flaw tolerance analysis. This approach was taken based upon the assumption that the critical flaw depths would be such as to be beyond the zone of any significant stresses introduced by this relatively small (the cavity liner thickness is 0.25 in.) fillet weld. The critical flaw depths calculated for this location appear to justify this assumption.

6.7.2 Maximum bending moment location (Node 4)

Node 4 is located within the concrete embedment at a point 16.5 in. from the point of application of the load. The beam geometry at this location is identical with that at the cavity liner interface, and the

Table 6.11

TROJAN: SUPPORT BEAM CRITICAL FLAW SIZE ANALYSIS

STRESS INTENSITY SUMMARY : INTERFACE WITH THE CAVITY LINER

Load case	Inner Concrete Effective ?	Flaw Aspect Ratio	-----Flaw Depth (in.)-----						
			0.00	0.05	0.10	0.20	0.30	0.60	1.25
DW+T+OBE	No	0.00	0.00	3891.00	5483.00	7964.00	10130.00	17252.00	55956.00
DW+T+OBE	No	0.10	0.00	3602.00	5081.00	7263.00	8990.00	13686.00	27757.00
DW+T+OBE	No	0.20	0.00	3322.00	4680.00	6577.00	7993.00	11582.00	19575.00
DW+T+OBE	No	0.30	0.00	2939.00	4140.00	5810.00	7061.00	9965.00	15101.00
DW+T+OBE	No	0.50	0.00	2393.00	3379.00	4741.00	5762.00	7968.00	10933.00
DW+T+OBE	Yes	0.00	0.00	N.A.	N.A.	N.A.	N.A.	N.A.	N.A.
DW+T+OBE	Yes	0.10	0.00	N.A.	N.A.	N.A.	N.A.	N.A.	N.A.
DW+T+OBE	Yes	0.20	0.00	N.A.	N.A.	N.A.	N.A.	N.A.	N.A.
DW+T+OBE	Yes	0.30	0.00	N.A.	N.A.	N.A.	N.A.	N.A.	N.A.
DW+T+OBE	Yes	0.50	0.00	N.A.	N.A.	N.A.	N.A.	N.A.	N.A.
DW+T+SSE	No	0.00	0.00	4292.00	6047.00	8783.00	11227.00	19026.00	61709.00
DW+T+SSE	No	0.10	0.00	3973.00	5604.00	8010.00	9915.00	15093.00	30611.00
DW+T+SSE	No	0.20	0.00	3664.00	5162.00	7253.00	8815.00	12773.00	21598.00
DW+T+SSE	No	0.30	0.00	3231.00	4552.00	6388.00	7764.00	10957.00	16603.00
DW+T+SSE	No	0.50	0.00	2667.00	3757.00	5272.00	6407.00	8860.00	12517.00
DW+T+SSE	Yes	0.00	0.00	N.A.	N.A.	N.A.	N.A.	N.A.	N.A.
DW+T+SSE	Yes	0.10	0.00	N.A.	N.A.	N.A.	N.A.	N.A.	N.A.
DW+T+SSE	Yes	0.20	0.00	N.A.	N.A.	N.A.	N.A.	N.A.	N.A.
DW+T+SSE	Yes	0.30	0.00	N.A.	N.A.	N.A.	N.A.	N.A.	N.A.
DW+T+SSE	Yes	0.50	0.00	N.A.	N.A.	N.A.	N.A.	N.A.	N.A.
DW+T+SB:LOCA	No	0.00	0.00	5545.20	7813.04	11347.94	14505.67	24580.88	79726.00
DW+T+SB:LOCA	No	0.10	0.00	5130.67	7237.40	10344.89	12804.16	19491.68	39530.94
DW+T+SB:LOCA	No	0.20	0.00	4728.56	6661.64	9360.75	11376.00	16484.56	27859.37
DW+T+SB:LOCA	No	0.30	0.00	4179.58	5888.17	8263.08	10041.71	14171.53	21474.39
DW+T+SB:LOCA	No	0.50	0.00	3407.38	4800.25	6736.23	8186.04	11319.89	15531.72
DW+T+SB:LOCA	Yes	0.00	0.00	N.A.	N.A.	N.A.	N.A.	N.A.	N.A.
DW+T+SB:LOCA	Yes	0.10	0.00	N.A.	N.A.	N.A.	N.A.	N.A.	N.A.
DW+T+SB:LOCA	Yes	0.20	0.00	N.A.	N.A.	N.A.	N.A.	N.A.	N.A.
DW+T+SB:LOCA	Yes	0.30	0.00	N.A.	N.A.	N.A.	N.A.	N.A.	N.A.
DW+T+SB:LOCA	Yes	0.50	0.00	N.A.	N.A.	N.A.	N.A.	N.A.	N.A.
DW+T+LB:LOCA	No	0.00	0.00	8784.00	12376.00	17974.00	22978.00	38939.00	126295.00
DW+T+LB:LOCA	No	0.10	0.00	8158.00	11507.00	16449.00	20359.00	30993.00	62856.00
DW+T+LB:LOCA	No	0.20	0.00	7473.00	10528.00	14794.00	17978.00	26052.00	44029.00
DW+T+LB:LOCA	No	0.30	0.00	6594.00	9290.00	13037.00	15843.00	22359.00	33881.00
DW+T+LB:LOCA	No	0.50	0.00	5364.00	7558.00	10606.00	12888.00	17823.00	24454.00
DW+T+LB:LOCA	Yes	0.00	0.00	N.A.	N.A.	N.A.	N.A.	N.A.	N.A.
DW+T+LB:LOCA	Yes	0.10	0.00	N.A.	N.A.	N.A.	N.A.	N.A.	N.A.
DW+T+LB:LOCA	Yes	0.20	0.00	N.A.	N.A.	N.A.	N.A.	N.A.	N.A.
DW+T+LB:LOCA	Yes	0.30	0.00	N.A.	N.A.	N.A.	N.A.	N.A.	N.A.
DW+T+LB:LOCA	Yes	0.50	0.00	N.A.	N.A.	N.A.	N.A.	N.A.	N.A.

DW+T+OBE = 1099 Kips.

DW+T+SSE = 1212 Kips

DW+T+SB:LOCA= 1558 Kips.

DW+T+LB:LOCA= 2443 Kips.

Fig. 6.22. Stress-intensity factors at cavity liner interface as function of flaw depth and flaw aspect ratio: DW + T + OBE loading.

Fig. 6.23. Stress-intensity factors at cavity liner interface as function of flaw depth and flaw aspect ratio: DW + T + SSE loading.

Fig. 6.24. Stress-intensity factors at cavity liner interface as function of flaw depth and flaw aspect ratio: DW + T + SBLOCA loading.

Fig. 6.25. Stress-intensity factors at cavity liner interface as function of flaw depth and flaw aspect ratio: DW + T + LBLOCA loading.

ORNL-DWG 88-4984 ETD

Fig. 6.26. Critical flaw depths at cavity liner interface after 7.48 EFPY of operation (current condition).

ORNL-DWG 88-4985 ETD

Fig. 6.27. Critical flaw depths at cavity liner interface after 32.0 EFPY of operation.

method of analysis is thus very similar to that described in Sect. 6.7.1 (only the input parameters are different). Examination of Fig. 6.7 and Table 6.8, however, shows the existence of a substantial difference between the two locations. The bending moment diagram of Fig. 6.7 shows a 57.6% increase (for the case where the inner concrete does not provide support for the beam) at Node 4 relative to the cavity liner interface, whereas the fracture-toughness data of Table 6.8 show only a 32% increase between the same two points at 32 EFY. It is to be anticipated therefore that critical flaw depths at the Node 4 location will be smaller than those at the cavity liner interface. To balance this view, however, it must be recognized that no attachments or processes with known crack-producing potential exist at Node 4, whereas a fillet weld between the cavity liner and the beam flange exists at the cavity liner interface.

A sample spreadsheet used in the analysis of the Node 4 stress-intensity factors is given in Table 6.12, and a summary of the stress-intensity-factor results is given in Table 6.13. Appendix 3 contains all of the spreadsheets used in the analysis of the Node 4 location.

Note that the results for Node 4 are influenced by the effectiveness of the internal concrete in providing foundation support for the beam. The results of Sect. 6.4.4.1 suggest that the inner concrete can be assumed effective for seismic loading cases but not for the double-ended break (LBLOCA) case. The SBLOCA case represents a transition condition.

Plots of the flaw-tip, stress-intensity factors are given in Figs. 6.28-6.31 for the case in which the inner concrete provides no foundation support and in Figs. 6.32-6.35 for the case where the inner concrete provides support. Cross plots of the critical flaw size as a function of the applied vessel support loading are given in Figs. 6.36 and 6.37 for the cases of no inner concrete support and in Figs. 6.38 and 6.39 for the case of fully effective inner concrete support.

Critical 32-EFYPY flaw depths for SBLOCA loading for the $a/l = 0$ case, with no inner concrete support, range from a maximum of 0.88 in. for the current condition (7.48 EFYPY) to a minimum of 0.61 in. at 32 EFYPY. With the inner concrete providing foundation support, these values increase to 0.97 and 0.65 in., respectively. Critical flaw depths at the maximum bending moment location are thus similar to those calculated for the cavity liner interface location. The trend of rapidly increasing critical flaw depths as the flaw aspect ratio increases above zero is also observed in the Node 4 analysis results.

6.7.3 Beam-flange grout-hole location

The grout-hole location in the beam flange differs substantially from the other locations considered because (1) the crack front of the flaw of concern is parallel to the axis of the hole rather than parallel to the free surface of the flange (as was the case in the previous two locations considered) and (2) the presence of the flange hole introduces a substantial stress concentration at the surface of the flange hole. Feature (1) requires that the stress at the middle plane of the beam flange be used in the stress-intensity-factor calculation to satisfy the

Table 6.12

TROJAN : SUPPORT BEAM CRITICAL FLAW SIZE ANALYSIS

Stress location :	:Node 4: Max. BM: Inner concrete not effective					
Flaw type	:Elliptical surface flaw					
Flaw aspect ratio a/l	:	0.100				
Plate thickness in.	:	2.500				
	:					
Stresses in beam flange without flaw :	:Unit load	:Load case	:Load case	:Max. surface	:Flange mean	:Flange bend.
	: (200 Kip) BM	: maximum load	: maximum BM	: stress	: stress	: stress
	:	:	:	:	:	:
Faulted (2) = DW + T + SB:LOCA	:	1527000.000	1558000.000	11895330.000	19509.781	16800.089
Faulted (3) = DW + T + LB:LOCA	:	1527000.000	2443000.000	18652305.000	30592.037	26343.143
	:					
	:					
Flaw shape parameter = Q	:	Sy =	40000.000	:Faulted(2)	(Sa + Sb)/Sy =	0.488
	:			:Faulted(3)	(Sa + Sb)/Sy =	0.765
	:					Q = 1.010
	:					Q = 0.970
	:					
Flaw depth "a" in.	:	0.050	0.100	0.200	0.300	0.600
Flaw depth to flange thickness ratio	:	0.020	0.040	0.080	0.120	0.240
Membrane correction factor Ma	:	1.100	1.100	1.120	1.140	1.250
Bending correction factor Mb	:	1.140	1.020	0.980	0.940	0.870
	:					
Faulted condition (2)	:					
	:					
(a) = $S_a \cdot M_a \cdot \sqrt{PI} \cdot \sqrt{a/Q}$	:	7287.919	10306.673	14840.852	18500.834	28688.739
(b) = $S_b \cdot M_b \cdot \sqrt{PI} \cdot \sqrt{a/Q}$	:	1111.354	1541.467	2094.475	2460.496	3220.542
KI = (a) + (b)	:	8399.273	11848.141	16935.328	20961.330	31909.282
	:					
Faulted condition (3)	:					
	:					
(a) = $S_a \cdot M_a \cdot \sqrt{PI} \cdot \sqrt{a/Q}$	:	11660.961	16491.089	23745.957	29602.074	45903.130
(b) = $S_b \cdot M_b \cdot \sqrt{PI} \cdot \sqrt{a/Q}$	:	1778.211	2466.409	3351.244	3936.892	5152.977
KI = (a) + (b)	:	13439.172	18957.499	27097.201	33538.966	51056.126

Table 6.13

TROJAN: SUPPORT BEAM CRITICAL FLAW SIZE ANALYSIS

STRESS INTENSITY SUMMARYRY : MODE 4 : LOCATION OF MAXIMUM BENDING MOMENT

Load Case	Inner Concrete Effective ?	Flaw Aspect Ratio	-----Flaw Depth (in.)-----						
			0.00	0.05	0.10	0.20	0.30	0.60	1.25
DW+T+OBE	No	0.00	0.00	6133.00	3641.00	12551.00	16043.00	27186.00	88178.00
DW+T+OBE	No	0.10	0.00	5677.00	8008.00	11444.00	14168.00	21568.00	43742.00
DW+T+OBE	No	0.20	0.00	5175.00	7291.00	10246.00	12452.00	18044.00	30495.00
DW+T+OBE	No	0.30	0.00	4617.00	6505.00	9129.00	11094.00	15657.00	23725.00
DW+T+OBE	No	0.50	0.00	3819.00	5380.00	7550.00	9175.00	12687.00	17408.00
DW+T+OBE	Yes	0.00	0.00	5327.00	7506.00	10902.00	13935.00	23615.00	76593.00
DW+T+OBE	Yes	0.10	0.00	4933.00	6959.00	9947.00	12312.00	18742.00	38012.00
DW+T+OBE	Yes	0.20	0.00	4501.00	6341.00	8910.00	10829.00	15692.00	26520.00
DW+T+OBE	Yes	0.30	0.00	4006.00	5644.00	7921.00	9626.00	13535.00	20536.00
DW+T+OBE	Yes	0.50	0.00	3313.00	4667.00	6550.00	7959.00	11007.00	15102.00
DW+T+SSE	No	0.00	0.00	6832.00	9626.00	13982.00	17872.00	30286.00	98232.00
DW+T+SSE	No	0.10	0.00	6348.00	8954.00	12799.00	15842.00	24116.00	48911.00
DW+T+SSE	No	0.20	0.00	5819.00	8198.00	11520.00	14000.00	20287.00	34286.00
DW+T+SSE	No	0.30	0.00	5139.00	7240.00	10160.00	12347.00	17426.00	26406.00
DW+T+SSE	No	0.50	0.00	4229.00	5958.00	8361.00	10160.00	14050.00	19278.00
DW+T+SSE	Yes	0.00	0.00	5934.00	8361.00	12143.00	15523.00	26305.00	85318.00
DW+T+SSE	Yes	0.10	0.00	5490.00	7745.00	11070.00	13702.00	20858.00	42303.00
DW+T+SSE	Yes	0.20	0.00	5021.00	7073.00	9939.00	12079.00	17504.00	29583.00
DW+T+SSE	Yes	0.30	0.00	4472.00	6301.00	8842.00	10746.00	15165.00	22980.00
DW+T+SSE	Yes	0.50	0.00	3676.00	5179.00	7268.00	8832.00	12214.00	16759.00
DW+T+SB:LOCA	No	0.00	0.00	9064.76	12772.01	18550.52	23712.47	40182.44	130328.35
DW+T+SB:LOCA	No	0.10	0.00	8399.27	11848.14	16935.33	20961.33	31909.28	64715.01
DW+T+SB:LOCA	No	0.20	0.00	7601.46	10709.00	15047.98	18237.62	26499.95	44795.66
DW+T+SB:LOCA	No	0.30	0.00	6732.55	9484.78	13310.32	16175.38	22827.78	36591.35
DW+T+SB:LOCA	No	0.50	0.00	5483.06	7724.43	10837.75	13172.75	18215.64	24993.20
DW+T+SB:LOCA	Yes	0.00	0.00	7787.17	10971.91	15936.00	20370.42	34519.10	111959.80
DW+T+SB:LOCA	Yes	0.10	0.00	7191.16	10143.96	14499.42	17946.34	27319.59	55406.68
DW+T+SB:LOCA	Yes	0.20	0.00	6530.38	9200.06	12927.67	15710.83	22764.00	38475.19
DW+T+SB:LOCA	Yes	0.30	0.00	5821.49	8201.29	11509.15	13986.51	19738.70	29910.41
DW+T+SB:LOCA	Yes	0.50	0.00	4756.07	6700.26	9402.52	11426.18	15800.44	21679.38
DW+T+LB:LOCA	No	0.00	0.00	14616.00	20594.00	29912.00	38235.00	64792.00	210149.00
DW+T+LB:LOCA	No	0.10	0.00	13439.00	18957.00	27097.00	33538.00	51056.00	103546.00
DW+T+LB:LOCA	No	0.20	0.00	12067.00	17000.00	23888.00	29031.00	42069.00	71097.00
DW+T+LB:LOCA	No	0.30	0.00	10694.00	15066.00	21143.00	25695.00	36262.00	54949.00
DW+T+LB:LOCA	No	0.50	0.00	8653.00	12190.00	17107.00	20789.00	28747.00	39443.00
DW+T+LB:LOCA	Yes	0.00	0.00	12476.00	17578.00	25531.00	32636.00	55304.00	179373.00
DW+T+LB:LOCA	Yes	0.10	0.00	11384.00	16059.00	22955.00	28412.00	43252.00	87719.00
DW+T+LB:LOCA	Yes	0.20	0.00	10363.00	14599.00	20514.00	24931.00	36127.00	61055.00
DW+T+LB:LOCA	Yes	0.30	0.00	9157.00	12900.00	18103.00	22000.00	31048.00	47048.00
DW+T+LB:LOCA	Yes	0.50	0.00	7457.00	10506.00	14743.00	17916.00	24775.00	33994.00
DW+T+OBE	= 1099 Kips.					DW+T+SB:LOCA= 1558 Kips.			
DW+T+SSE	= 1212 Kips.					DW+T+LB:LOCA= 2443 Kips.

Fig. 6.28. Stress-intensity factors at maximum bending-moment location as function of flaw depth and flaw aspect ratio: DW + T + OBE loading: no inner concrete support.

Fig. 6.29. Stress-intensity factors at maximum bending-moment location as function of flaw depth and flaw aspect ratio: DW + T + SSE loading: no inner concrete support.

Fig. 6.30. Stress-intensity factors at maximum bending-moment location as function of flaw depth and flaw aspect ratio: DW + T + SBLOCA loading: no inner concrete support.

Fig. 6.31. Stress-intensity factors at maximum bending-moment location as function of flaw depth and flaw aspect ratio: DW + T + LBLOCA loading: no inner concrete support.

Fig. 6.32. Stress-intensity factors at maximum bending-moment location as function of flaw depth and flaw aspect ratio: DW + T + OBE loading: with inner concrete support.

Fig. 6.33. Stress-intensity factors at maximum bending-moment location as function of flaw depth and flaw aspect ratio: DW + T + SSE loading: with inner concrete support.

Fig. 6.34. Stress-intensity factors at maximum bending-moment location as function of flaw depth and flaw aspect ratio: DW + T + SBLOCA loading: with inner concrete support.

Fig. 6.35. Stress-intensity factors at maximum bending-moment location as function of flaw depth and flaw aspect ratio: DW + T + LBLOCA loading: with inner concrete support.

Fig. 6.36. Critical flaw depths at maximum bending-moment location after 7.4 EFPY of operation (current condition): no inner concrete support.

Fig. 6.37. Critical flaw depths at maximum bending-moment location after 32 EFPY of operation: no inner concrete support.

Fig. 6.38. Critical flaw depths at maximum bending-moment location after 7.48 EFY of operation: with inner concrete support.

Fig. 6.39. Critical flaw depths at maximum bending-moment location after 32 EFY of operation: with inner concrete support.

requirement for plane-strain constraint at the crack tip. Feature (2) requires that the nonlinear stress distribution in the flange adjacent to the grout hole be factored into the stress-intensity-factor calculations.

6.7.3.1 Flaw-tip stress-intensity-factor equations. Solutions exist in the literature for the calculations of stress-intensity factors at the tip of cracks emanating from round holes in both finite- and infinite-width plates. The solution used in this analysis was taken from Ref. 20 and has the following form:

$$K_I = \sigma \sqrt{\pi a} \times F, \quad (10)$$

where

σ = the uniform far-field tensile stress on the plate (ksi);

a = the half width of a symmetrical radial crack, including the hole radius;

F = a factor reflecting both the concentration of stress adjacent to the hole and the finite width of the plate.

The factor F is obtained from the curves of Ref. 20, which are reproduced in Fig. 6.40. Note that factor F is a function of three variables, one of which (h/b) is defined as follows:

$$\frac{h}{b} = \frac{\text{distance from crack plane to the uniformly loaded plate edge}}{\text{plate half-width}}. \quad (11)$$

The geometry of the beam flange and the postulated flaws fits the conditions of the K_I solution exactly, but the loading condition does not. Tensile end loads in the beam flange are generated by shear flows at the flange to shear web interface. This leads to (1) a nonuniform axial distribution of end load along the length of the flange and (2) shear stresses in the plane of the flange because of the web location. A comparison of the two plate loading conditions is given in Fig. 6.41.

The effect of the departure from prototypic loading on the calculated K_I values is not known at this time, but a potential exists for the calculated K_I values, and the associated critical crack depths, to be nonconservative.* This fact should be recognized when evaluating the analysis results, particularly for cases involving F values taken from the knee of the curve of Fig. 6.40.

*Late in the preparation of this report, both cases described in Fig. 6.41 were calculated using two-dimensional finite-element analysis. These analyses checked the "exact" solution for problem (b) within 0.6% and indicate a 5% higher K_I value for problem (a) than for problem (b). The 5% increase in K_I reduces the critical flaw size for the SBLOCA loading condition by 0.1 in. This work is discussed in detail in Appendix 6.

Fig. 6.40. Stress-intensity-factor equation used at beam-flange hole location. Source: Ref. 20.

Fig. 6.41. Comparison of beam-flange boundary conditions with boundary conditions used in the Newman analysis. (a) Beam-flange loading and (b) Newman analysis loading.

6.7.3.2 Flaw-tip stress-intensity factors. A sample spreadsheet used in the analysis of the beam flange hole radial flaws is given in Table 6.14. The mean stress entry in this table is based on the *full* section properties at the hole centerline (i.e., the reduction in section modulus because of the loss of flange area was not included). This approach generated a pseudo far-field stress corresponding to that defined in Fig. 6.40. Note that the beam flange end load at the plane of the hole centerline corresponds exactly with that given by the correct section properties (i.e., the bending moment calculation includes the effect of the hole).

A complete set of spreadsheets for the flaw tolerance analysis of the beam flange holes is provided in Appendix 4. A summary of flaw-tip, stress-intensity factors for the beam flange hole is given in Table 6.15.

Plots of the flaw-tip, stress-intensity factors for each of the load combinations considered are given in Figs. 6.42–6.45. Superposition of the design fracture-toughness values from Table 6.8 onto these curves defined limiting flaw depths for each load case considered. These limiting values are summarized, along with critical crack lengths for the other locations analyzed, in Table 6.16.

6.7.3.3 Lifetime critical-flaw-depth analyses. The beam flange hole location was selected for a study of the lifetime variation in critical flaw depths on the basis of its having the smallest critical flaw size at the end of life. NDTT shifts corresponding to four intermediate times in the remaining plant life were calculated using the method of Sect. 6.6 and translated into design dynamic fracture-toughness values using the design curve of Fig. 6.18. A plot of the resulting lifetime variation in the design fracture toughness is given in Fig. 6.21.

Fracture-toughness design values corresponding to intermediate plant operating periods were then superposed on the curves of Figs. 6.42–6.45 to generate critical flaw depths. Plots of the resulting critical flaw depth as a function of reactor operating period are given in Fig. 6.46 for the SBLOCA loading condition. Note that the reduction in critical flaw depth between the current time and 20 EFPY into the design life occurs as an approximately linear function of the plant equivalent full-power operating period.

Plots of critical flaw depth as a function of the maximum dynamic load on the vessel support are given in Figs. 6.47 and 6.48. The data used to construct these plots are summarized in Table 6.17.

The flaw considered in the beam flange hole analysis is a constant-depth, through-flange flaw and thus corresponds to the $a/l = 0$ case analyzed for the other two critical locations on the support beam. Note that the average slope of the curve for 32 EFPY between the DW + T + OBE and DW + T + SBLOCA loading cases is -2.6×10^{-3} in./kip (see Fig. 6.47). The corresponding slope for the cavity liner interface location is obtained from Fig. 6.27 as -3.3×10^{-4} in./kip. The slopes are seen to differ by a factor of 7.88. Benefits to be derived from a reduction in the beam flange stress are thus greater at the beam flange hole location than at the other locations.

Factors contributing to the 7.88 Δa /kip slope differential are identified and discussed in Sect. 6.8.

Table 6.14

TROJAN : SUPPORT BEAM CRITICAL FLAW SIZE ANALYSIS

Stress location :	4 in. diameter hole in the upper beam flange: inner concrete effective									
Flaw type	Uniform depth radial flaw									
Hole diameter in.	4.000									
Plate width in.	16.000									
Stresses in beam flange without flaw :	Unit load (200 Kip) BM	Load case maximum load	Load case maximum BM	Max. surface stress	Flange mean stress:Note 1	Flange bend. stress				
Normal + Upset = DW + T + OBE	1238000.000	1099000.000	6802810.000	11157.432	9607.788	1549.643				
Faulted (1) = DW + T + SSE	1238000.000	1212000.000	7502280.000	12304.647	10595.668	1708.979				
Flaw depth "a" in.	0.050	0.100	0.150	0.200	0.300	0.400	0.600	1.000	2.000	3.000
Flaw depth to flange width ratio (R+a)/W	0.256	0.263	0.269	0.275	0.288	0.300	0.325	0.375	0.500	0.625
Membrane correction factor = F (Ref. 24)	0.520	0.730	0.840	0.910	1.010	1.065	1.150	1.200	1.300	1.450
Normal plus upset condition										
$K_I = S_m F \sqrt{2SORT(P)} \sqrt{2SORT(R+a)}$	12678.814	18014.859	20974.754	22985.349	26084.568	28096.588	31577.873	35394.863	44276.340	55214.274
Faulted condition (1)										
$K_I = S_m F \sqrt{2SORT(P)} \sqrt{2SORT(R+a)}$	13982.459	19867.160	23131.394	25348.719	28766.603	30985.501	34824.734	39034.189	48828.866	60891.447
Notes : (1)	This stress is based on the full flange width in order to match the uniform far field stress boundary condition incorporated into the stress intensity factor equation. (The flange hole has not been taken out)									

Table 6.15

TROJAN: SUPPORT BEAM CRITICAL FLAW SIZE ANALYSIS

STRESS INTENSITY SUMMARY

4 IN. DIAMETER HOLE IN THE UPPER BEAM FLANGE

Load case	Inner Concrete Effective ?	Flaw Aspect Ratio	-----Flaw Depth (in.)-----										
			0.000	0.050	0.100	0.150	0.200	0.300	0.400	0.600	1.000	2.000	3.000
DW-T-0BE	No	0.000	0.000	15290.363	21725.513	25295.079	27719.811	31457.399	33883.850	38082.201	42685.404	53396.264	66587.166
DW-T-0BE	Yes	0.000	0.000	12678.814	18014.859	20974.754	22985.349	26084.568	28096.588	31577.873	35394.863	44276.340	55214.274
DW-T-1SE	No	0.000	0.000	16862.530	23959.346	27895.938	30569.982	34691.873	37367.813	41997.841	47074.349	58886.508	73433.708
DW-T-1SE	Yes	0.000	0.000	13982.459	19867.160	23131.394	25348.719	28766.603	30985.501	34824.734	39034.189	48828.866	60891.447
DW-T-SB:LOCA	No	0.000	0.000	21676.420	30799.225	35859.630	39297.056	44595.658	48035.522	53987.324	60513.066	75697.343	94397.456
DW-T-SB:LOCA	Yes	0.000	0.000	17974.151	25538.808	29734.911	32585.235	36978.851	39831.196	44766.448	50177.613	62768.460	78274.649
DW-T-LB:LOCA	No	0.000	0.000	33989.406	48294.292	56229.188	61619.197	69927.595	75321.425	84654.064	94886.663	118696.154	148018.604
DW-T-LB:LOCA	Yes	0.000	0.000	28184.116	40045.770	46625.409	51094.819	57984.168	62456.748	70195.400	78680.300	98423.201	122737.462

Fig. 6.42. Stress-intensity factors at beam-flange grout-hole location as function of flaw depth and vessel support loading: no inner concrete support.

Fig. 6.43. Stress-intensity factors at beam-flange grout-hole location as function of flaw depth and vessel support loading: no inner concrete support.

Fig. 6.44. Stress-intensity factors at beam-flange grout-hole location as function of flaw depth and vessel support loading: with inner concrete support.

Fig. 6.45. Stress-intensity factors at beam-flange grout-hole location as function of flaw depth and vessel support loading: with inner concrete support.

Table 6.16

SUMMARY OF CRITICAL CRACK LENGTHS

				Present Condition					End of Life				
				7.48 EFPY					32 EFPY				
Location On Beam	Loading Condition	Flaw Type	Inner Concrete Effective ?	-----Flaw Aspect Ratio-----					-----Flaw Aspect Ratio-----				
				0.0	0.1	0.2	0.3	0.5	0.0	0.1	0.2	0.3	0.5
				-----Flaw Depth-----					-----Flaw Depth-----				
				A0	A1	A2	A3	A5	A0	A1	A2	A3	A5
Cavity liner interface	DW + T + OBE	Surface : Elipsoidal	N.A.	1.13					0.83				
Cavity liner interface	DW + T + SSE	Surface : Elipsoidal	N.A.	1.05					0.78	1.26			
Cavity liner interface	DW + T + SB:LOCA	Surface : Elipsoidal	N.A.	0.89					0.68	0.97			
Cavity liner interface	DW + T + LB:LOCA	Surface : Elipsoidal	N.A.	0.68	0.97				0.44	0.59	0.77	1.08	
Node 4 : Max. Bending Mt.	DW + T + OBE	Surface : Elipsoidal	No	1.15					0.74	1.17			
Node 4 : Max. Bending Mt.	DW + T + SSE	Surface : Elipsoidal	No	1.07					0.70	1.03			
Node 4 : Max. Bending Mt.	DW + T + SB:LOCA	Surface : Elipsoidal	No	0.88					0.61	0.78	1.11		
Node 4 : Max. Bending Mt.	DW + T + LB:LOCA	Surface : Elipsoidal	No	0.67	0.94				0.33	0.43	0.57	0.76	1.30
Flange grout hole	DW + T + OBE	Twin radial cracks	No						1.60				
Flange grout hole	DW + T + SSE	Twin radial cracks	No						1.17				
Flange grout hole	DW + T + SB:LOCA	Twin radial cracks	No						0.42				
Flange grout hole	DW + T + LB:LOCA	Twin radial cracks	No	0.93					0.10				
Node 4 : Max. Bending Mt.	DW + T + OBE	Surface : Elipsoidal	Yes	1.28					0.82				
Node 4 : Max. Bending Mt.	DW + T + SSE	Surface : Elipsoidal	Yes	1.18					0.77	1.22			
Node 4 : Max. Bending Mt.	DW + T + SB:LOCA	Surface : Elipsoidal	Yes	0.97					0.65	0.92			
Node 4 : Max. Bending Mt.	DW + T + LB:LOCA	Surface : Elipsoidal	Yes	0.73	1.14				0.41	0.55	0.72	1.00	
Flange grout hole	DW + T + OBE	Twin radial cracks	Yes						2.44				
Flange grout hole	DW + T + SSE	Twin radial cracks	Yes						2.00				
Flange grout hole	DW + T + SB:LOCA	Twin radial cracks	Yes						0.90				
Flange grout hole	DW + T + LB:LOCA	Twin radial cracks	Yes	1.73					0.18

Fig. 6.46. Lifetime variation of critical flaw depths at beam-flange grout-hole location for DW + T + SBLOCA loading.

Fig. 6.47. Lifetime variation of critical flaw depths at beam-flange grout-hole location for range of vessel support beam loading conditions: no inner concrete support.

Fig. 6.48. Lifetime variation of critical flaw depths at beam-flange grout-hole location for range of vessel support beam loading conditions: with inner concrete support.

6.7.3.4 The impact of NDTT variations. Variations in reported values of NDTT for A36 were reviewed in Sect. 6.5.5, and it was shown that a further temperature shift of 11°F must be applied to the results calculated in this report to encompass the 90% confidence limit on the NDTT population. The carpet plot of Fig. 6.49 has been constructed to permit a rapid evaluation of the impact of this temperature shift on critical flaw depths at the beam flange grout hole location for the governing case where the inner concrete provides no support to the beams. Note that the plot of Fig. 6.49 can also be used to assess the impact of the departure of the beam operating temperature from the 90°F value used in the analysis. Analyses used to construct the plot of Fig. 6.49 are summarized in Table 6.18.

As an example of the use of Fig. 6.49, consider the case of the 32-EFPY critical flaw depth for the SBLOCA loading condition. This value (0.42 in.) is shown entered on the SBLOCA load curve in Fig. 6.49 at a $T - \text{NDTT}$ value of -13°F as point A. (See Table 6.8 for the origin of the -13°F .) Application of the 11°F temperature shift required to encompass the 90% confidence limit on NDTT moves the calculated $T - \text{NDTT}$ value to -24°F , corresponding to point B on Fig. 6.49. The critical flaw depth corresponding to point B is seen to be 0.31 in. Application of the 90% NDT confidence limit temperature shift (11°F) thus reduced the critical flaw depth by 0.11 in.

Table 6.17. Lifetime variation of critical flaw sizes at the beam flange hole location

E.F.P.Y.	7.48E+00	1.00E+01	1.30E+01	2.00E+01	2.50E+01	3.20E+01
Total D.P.A.	2.61E-05	3.17E-05	4.29E-05	5.41E-05	6.53E-05	8.09E-05
	1.11E-13	1.01E-13	9.07E-14	8.57E-14	8.28E-14	8.02E-14
RT NDT Shift	3.02E+01	3.60E+01	4.60E+01	5.50E+01	6.40E+01	7.50E+01
Design fracture toughness	9.27E+01	8.17E+01	7.08E+01	6.04E+01	5.43E+01	4.86E+01
Limiting Flaw Sizes : No Inner Concrete						
DW + T + OBE				2.49E+00	2.01E+00	1.60E+00
DW + T + SSE			2.81E+00	2.07E+00	1.57E+00	1.17E+00
DW + T + SB:LOCA		2.31E+00	1.69E+00	9.70E-01	6.10E-01	4.20E-01
DW + T + LB:LOCA	9.3CE-01	5.30E-01	3.20E-01	1.85E-01	1.35E-01	1.00E-01
Limiting Flaw Sizes : With Inner Concrete						
DW + T + OBE				2.88E+00	2.42E+00	
DW + T + SSE				2.94E+00	2.40E+00	2.00E+00
DW + T + SB:LOCA		3.20E+00	2.51E+00	1.79E+00	1.32E+00	9.00E-01
DW + T + LB:LOCA	1.73E+00	1.18E+00	6.00E-01	3.50E-01	2.50E-01	1.75E-01

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Fig. 6.49. Carpet plot used to assess the impact on critical flaw depth of variations in NDTT, support operating temperature, and support load. Point B represents a $-11^\circ F$ shift in NDTT, while point C represents a $10^\circ F$ increase in support operating temperature.

Table 6.18

CRITICAL FLAW DEPTHS AS A FUNCTION OF SUPPORT LOAD & (T - NDT)

BEAM FLANGE GROUT HOLE : NO INNER CONCRETE

		-----LOADING CONDITION-----			
		OBE	SSE	SB:LOCA	LB:LOCA
		1099.000	1212.000	1558.000	2443.000
		(Kips.)	(Kips.)	(Kips.)	(Kips.)
T - NDT	Design	-----CRITICAL FLAW DEPTH (in.)-----			
(Deg.F.)	K1d (KSI* $\sqrt{IN}$)				
40.000	113.986				1.720
30.000	88.926			2.690	0.750
20.000	73.199		2.990	1.830	0.360
10.000	62.842	2.700	2.240	1.150	0.210
0.000	55.610	2.130	1.680	0.650	0.140
-10.000	50.010	1.690	1.260	0.470	0.115
-20.000	45.700	1.300	0.890	0.320	0.085
-30.000	42.270	1.100	0.720	0.275	0.078
-40.000	39.376	0.720	0.480	0.205	0.065
-50.000	37.636	0.550	0.380	0.160	0.060
-60.000	36.400	0.480	0.330	0.135	0.055
-----P + 100*(T-NDT)-----					
40.000					6443.000
30.000				4558.000	5443.000
20.000			3212.000	3558.000	4443.000
10.000	2099.000	2212.000	2558.000	3443.000	
0.000	1099.000	1212.000	1558.000	2443.000	
-10.000	99.000	212.000	558.000	1443.000	
-20.000	-901.000	-788.000	-442.000	443.000	
-30.000	-1901.000	-1788.000	-1442.000	-557.000	
-40.000	-2901.000	-2788.000	-2442.000	-1557.000	
-50.000	-3901.000	-3788.000	-3442.000	-2557.000	
-60.000	-4901.000	-4788.000	-4442.000	-3557.000	

In a further example, if the beam operating temperature is increased by 10°F to 100°F and the temperature shift associated with the 90% NDTT confidence limit is neglected, then T - NDTT changes from -13°F to -3°F. Point C on Fig. 6.49 corresponds to the -3°F T - NDTT value. The corresponding critical flaw depth is obtained as 0.6 in., an increase of 0.18 in.

6.8 Evaluation of Results

Flaws can be present in the support beams because of cracking induced by the fabrication processes and/or cracking induced by fatigue loading. In both cases the flaws will grow as a result of the effect of cyclic loading on the supports. The magnitude of the flaw growth is of concern in the evaluation because it contributes an increment to the flaw dimensions that would not have been present during final inspection of the support beams.

6.8.1 Flaw growth as a result of cyclic loading

Cyclic crack-growth data were taken from Ref. 7 and are reproduced in Fig. 6.50. The data for weldments were selected for use in this evaluation because (1) the fillet weld at the cavity liner interface and the flame-cut surface of the grout hole will both be in a metallurgical condition that is representative of weld metal/heat-affected zone (HAZ) and (2) crack-growth rates for the welded specimens were higher than those for parent material. A lower bound curve was added to the data of Fig. 6.50 and used to generate the following equation:

$$da/dN = 1.158 \times 10^{-13} \times \Delta K^{5.837} \quad (12)*$$

*Other formulations of the equation for crack growth in A36 have been evaluated (Ref. 21). While these alternative formulations do give slightly different crack-growth predictions, the differences are too small to influence the conclusions drawn here.

Fig. 6.50. Fatigue crack-growth rates for A36.

A definition of the cyclic loading to be included in the crack-growth analysis is given in Sect. 6.3.3.

Critical flaw dimensions are given in Table 6.16 for the SBLOCA loading. These flaws all represent conditions for which the flaw-tip, stress-intensity factor equals the local value of K_{Id} . To rapidly obtain an upper bound, order-of-magnitude assessment of flaw growth as a result of cyclic loading, the flaw-tip stress-intensity factors for all cycles in a given loading condition were calculated using the critical flaw geometry. Results were then summed to give a cumulative flaw growth for the total of all of the support beam duty cycle events.

Results from the upper bound flaw-growth analysis are summarized in Table 6.19. Flaw growth is insignificant relative to the dimensions of the critical flaws. This result leads to the conclusion that presumably the critical flaws identified in Table 6.16 will exist in the support beams only if they escaped detection during final inspection.

6.8.2 Interface with the reactor cavity liner

Critical flaw depths given for this location in Table 6.16 increase rapidly as the flaw aspect ratio (a/l) increases, reaching values in excess of $t/2$ for a flaw aspect ratio of 0.2 (SBLOCA loading). The significance of this finding is that a substantial length of flaw would have been visible on the surface of the component at final inspection. The specific lengths of relevance are 16.0 in. for the $a/l = 0$ case and 9.7 in. for the $a/l = 0.1$ case. Detection of surface indications of this magnitude presumably is well within the capability of a trained inspector using visual inspection techniques. Dye penetrant or magnetic particle inspection presumably would have detected such large surface indications. It is considered unlikely therefore that flaws with these critical depths and aspect ratios could exist in the support beams at the liner interface location. It is recommended that the Trojan inspection procedures (and inspection records if they are available) be reviewed to confirm this tentative conclusion.

6.8.3 Maximum bending moment location (Node 4)

Conclusions reached for this location are identical with those previously given for the cavity liner interface. Surface lengths of flaws having the potential to produce brittle fracture under SBLOCA loading were all in excess of 5.0 in. In this instance no weld is present, and therefore a transverse (normal to the rolling direction) surface crack of this magnitude presumably would have been readily apparent to the inspector.

6.8.4 Beam top-flange grout-hole location

The stress concentration produced by this large hole (4-in. diam) in a highly stressed portion of the beam upper flange results in a critical flaw depth for the SBLOCA event of only 0.42 in. (if the inboard concrete is not effective). It is considered unlikely that any

Table 6.19

ORDER OF MAGNITUDE FATIGUE CRACK GROWTH ANALYSIS

Event	Refueling	O.B.E.	S.S.E.	Small Break L.O.C.A.	Totals a + b + c	Totals a + b + d
	(a)	(b)	(c)	(d)		
Number Of Cycles	4.0000E+01	4.0000E+02	2.0000E+01	5.0000E+00		
Max Load = Pmax	8.1800E+02	1.0990E+03	1.2120E+03	1.5580E+03		
Min Load = Pmin	3.9500E+02	5.3700E+02	4.2400E+02	7.8000E+01		
Load Range = Pmax - Pmin	4.2300E+02	5.6200E+02	7.8800E+02	1.4800E+03		
Load Ratio = dP/Pmax = R	5.1711E-01	5.1137E-01	6.5017E-01	9.4994E-01		
CAVITY LINER INTERFACE						
K1c (KSI)	3.1000E+01	3.1000E+01	3.1000E+01	3.1000E+01		
dK = R * K1c	1.6031E+01	1.5853E+01	2.0155E+01	2.9448E+01		
dA/dN = 1.158E-13*dK^5.837	1.2502E-06	1.1714E-06	4.7578E-06	4.3510E-05		
N * dA/dN	5.0010E-05	4.6855E-04	9.5156E-05	2.1755E-04	6.1371E-04	7.3611E-04
NODE 4						
K1c (KSI)	4.1100E+01	4.1100E+01	4.1100E+01	4.1100E+01		
dK = R * K1c	2.1253E+01	2.1017E+01	2.6722E+01	3.9042E+01		
dA/dN = 1.158E-13*dK^5.837	6.4850E-06	6.0759E-06	2.4679E-05	2.2569E-04		
N * dA/dN	2.5940E-04	2.4304E-03	4.9357E-04	1.1284E-03	3.1833E-03	3.8182E-03
BEAM FLANGE HOLE						
K1c (KSI)	4.8600E+01	4.8600E+01	4.8600E+01	4.8600E+01		
dK = R * K1c	2.5132E+01	2.4853E+01	3.1598E+01	4.6167E+01		
dA/dN = 1.158E-13*dK^5.837	1.7251E-05	1.6163E-05	6.5648E-05	6.0035E-04		
N * dA/dN	6.9004E-04	6.4650E-03	1.3130E-03	3.0018E-03	8.4681E-03	1.0157E-02

practical inspection technique applied at the time of fabrication could have reliably detected so small a flaw in and adjacent to the flame-cut surface.

The small size of the critical flaw at the flange grout hole makes it imperative that a reliable characterization be obtained of the flaws present in this region. The fact that this region of the support beam is embedded within the cavity wall makes direct inspection impossible. It is suggested therefore that a generic definition of the population of flaws adjacent to the surface of flame-cut holes in plates of similar thickness be compiled using data from (1) the manufacturers of flame-cutting equipment, (2) destructive examination of any available flame-cut, off-cut material, and (3) duplication of the flange hole flame-cutting operation and destructive examination of the finished product.

6.8.5 Compatibility check on the stress-intensity-factor equations

Two separate sources were used for the stress-intensity-factor equations used in this analysis. Postulated flaws in sections of the beam flange remote from discontinuities were analyzed using equations and factors taken from Sect. XI, Appendix A of the *ASME Boiler and Pressure Vessel Code*,¹⁹ whereas flaws at the beam flange hole location were analyzed using equations from the *Stress Analysis of Cracks Handbook*.²⁰ The purpose of the check summarized in this section is to verify that these equations, as interpreted and utilized in this report, give compatible results.

The stress-intensity-factor equations taken from Ref. 19 were for a single-sided surface flaw, whereas those taken from Ref. 20 were for symmetric radial flaws emanating from a circular hole in a finite plate. To provide a basis for the compatibility check, the beam flange hole case was divided on its axis of symmetry and idealized as a plate with a single-sided flaw. Plate "thicknesses" were defined as the surface-to-surface distance in the direction of crack propagation. This definition gave a value of $t = 2.5$ in. for the locations remote from the hole and $t = 8.0$ in. for the hole location. The value 8.0 in. (one-half the flange width) is used rather than the ligament width (6.0 in.) because the stress-intensity-factor equations of Ref. 20 define the crack length to include the hole radius.

With these analytical models defined, the criteria for compatibility were defined as follows. When applied in a compatible manner on a finite-width plate, the two equations must predict compatible stress-intensity/flaw-depth relationships. This requirement specifically excludes cracking regimes in which nonlinear behavior would be anticipated such as (1) the area of concentrated stress immediately adjacent to the hole and (2) secondary bending effects resulting from large-scale penetration of single-sided cracks. This latter constraint on the compatibility comparison was necessary because one of the models (the beam flange hole model) is constrained from generating secondary bending moments while the other model is not.

The compatibility check was made using the $a/l = 0$ SBLOCA results for the cavity liner interface and the beam flange hole. These results are reproduced in Table 6.20, column 7.

Table 6.20

STRESS INTENSITY FACTOR EQUATION COMPATIBILITY CHECK

1	2	3	4	5	6	7	8	9	10
Beam Location	Actual Flaw Depth	Effective Flaw Depth	Effective Beam Thickness	"a"/"t"	Stress Intensity Factor ----- DW+T+OBE DW+T+SB:LOCA		Flange Mean Stress	Normalized SIF KI/KSI	Design Stress Intensity factor
	a	"a"	"t"		KSI*(in.) ^{1/2}		KSI	KI/KSI	
Flange Hole	0.00	2.00	8.00	0.25	0.00	0.00	16.43	0.00	31.00
Flange Hole	0.05	2.05	8.00	0.26	15.29	21.68	16.43	1.32	31.00
Flange Hole	0.10	2.10	8.00	0.26	21.72	30.80	16.43	1.87	31.00
Flange Hole	0.15	2.15	8.00	0.27	25.29	35.86	16.43	2.18	31.00
Flange Hole	0.20	2.20	8.00	0.28	27.62	39.30	16.43	2.39	31.00
Flange Hole	0.30	2.30	8.00	0.29	31.45	44.60	16.43	2.71	31.00
Flange Hole	0.40	2.40	8.00	0.30	33.88	48.04	16.43	2.92	31.00
Flange Hole	0.60	2.60	8.00	0.33	38.08	54.00	16.43	3.29	31.00
Flange Hole	1.00	3.00	8.00	0.38	42.68	60.51	16.43	3.68	31.00
Flange Hole	2.00	4.00	8.00	0.50	53.39	75.70	16.43	4.61	31.00
Flange Hole	3.00	5.00	8.00	0.63	66.59	94.40	16.43	5.75	31.00
Cavity Liner	0.00	0.00	2.50	0.00	0.00	0.00	10.66	0.00	48.60
Cavity Liner	0.05	0.05	2.50	0.02	3.89	5.54	10.66	0.52	48.60
Cavity Liner	0.10	0.10	2.50	0.04	5.48	7.81	10.66	0.73	48.60
Cavity Liner	0.20	0.20	2.50	0.08	7.96	11.34	10.66	1.06	48.60
Cavity Liner	0.30	0.30	2.50	0.12	10.18	14.51	10.66	1.36	48.60
Cavity Liner	0.60	0.60	2.50	0.24	17.25	24.58	10.66	2.31	48.60
Cavity Liner	1.25	1.25	2.50	0.50	55.96	79.73	10.66	7.48	48.60

The stress-intensity factors of Table 6.20 were produced by differing flange stresses, because the flange bending moment varies along the length of the beam. To provide a basis for comparison therefore the stress-intensity factors were normalized by dividing by the local flange far-field membrane stress (column 8). The resulting parameter has the units of $\text{ksi}/\sqrt{\text{in.}}/\text{ksi}$ or $\sqrt{\text{in.}}$ (column 9).

A similar normalizing procedure was applied to the flaw depth to beam thickness ratio. In the case of the beam flange hole location, both the plate thickness "t" and the flaw depth "a" are pseudo values, having the geometric configuration previously defined. The resulting dimensionless a/t values are given in column 5 of Table 6.20.

The data of columns 5 and 9 of Table 6.20 were then plotted to produce the curves of Fig. 6.51. The linear portions of those curves are clearly predicting similar relationships, and the equations, as utilized in this report, are therefore considered compatible. Note that the portion of the cavity liner interface curve extending from $a/t = 0.24$ to $a/t = 0.5$ was defined by a single point and would not be expected to be linear over its entire length.

Fig. 6.51. Compatibility check on the stress-intensity-factor equations used in flaw tolerance analysis. Compatibility is demonstrated by slope continuity in linear portions of curves.

6.8.6 Load sensitivity comparison

As noted in Sect. 6.7.3.3, the rate of decrease of allowable flaw size corresponding to a given increase in support load differed by a factor of almost 8 between the beam flange hole and the cavity liner interface locations. Higher rates of change were obtained at the beam flange hole location. The representation of the flaw depth vs the stress-intensity-factor relationship given in Fig. 6.51 provides a basis for identifying the parameters responsible for the bulk of the factor of 8 rate change.

Stress-intensity factors for the beam flange hole and cavity liner interface locations are plotted as a function of the dimensionless flaw depth parameter a/t in Fig. 6.52. Curves are plotted for both the DW + T + OBE and DW + T + SBLOCA cases. The linear portions of the curves have differing slopes, reflecting (1) the differing beam flange membrane stress levels at the two locations for a given support load because of the variation of bending moments along the beam and (2) increases in support load between the DW + T + OBE and the DW + T + SBLOCA cases.

Fig. 6.52. Comparison of the critical-flaw-depth sensitivity to support load variations at cavity liner interface and beam-flange grout-hole locations. The beam-flange grout-hole location shows greater sensitivity.

End-of-life design, stress-intensity-factor lines were superposed on the two sets of curves and the dimensionless flaw depth increments $\Delta(a/t)$ between the DW + T + SBLOCA, and the DW + T + OBE curves were determined from the intercepts as follows:

Location	$\Delta(a/t)$
Cavity liner interface	0.06
Beam flange hole	0.144

In the case of the cavity liner interface, both the a and t values correspond directly to the actual flaw depth and the actual beam flange thickness. The flaw depth increment corresponding to the $\Delta(a/t)$ value of 0.06 on the 2.5-in.-thick flange can thus be obtained as follows:

$$\Delta a_{\text{CLI}} = 2.5 \times 0.06 = 0.15 \text{ in.}$$

At the beam flange hole location, the pseudo crack length a corresponds to $R + a$, where R is the hole radius and a is the depth of a uniform flaw measured from the surface of the hole. The thickness of the pseudo beam measured in the direction of propagation of the crack is in this case the half-width of the plate (8.0 in.). The flaw depth increment at the hole location corresponding to the $\Delta(a/t)$ value of 0.144 is thus obtained as follows:

$$\Delta(R + a) = 0.144 \times 8.0 = 1.152 .$$

Because the hole radius R is constant, the value Δa_{BFH} is equal to 1.152. The ratio of the critical flaw depth change at the cavity liner interface (CLI) and the beam flange hole (BFH) locations for the same increment in applied loading is thus obtained as follows:

$$\frac{\Delta a_{BFH}}{\Delta a_{CLI}} = \frac{1.152}{0.15} = 7.68 .$$

This value is very close to the value noted in Sect. 6.7.3.3. The rate difference is therefore seen to be caused by differences in the following governing parameters between the two locations:

Parameter	Cavity liner	Beam flange hole
Bending moment/beam	4.845 lb in./lb	7.465 lb in./lb
Thickness in direction of crack propagation	2.5 in.	8.0 in.
End-of-life design fracture toughness	31.0 ksi $\sqrt{\text{in.}}$	48.6 ksi $\sqrt{\text{in.}}$

6.8.7 Residual stress effects

6.8.7.1 Analysis approach. Residual stresses will be generated within the support beam elements by both the welding and flame-cutting operations. Postweld stress relief will reduce but not eliminate the residual stresses. The residual stresses are important because they add to the stresses produced by external loading of the support beams and thereby increase the stress-intensity factors at the tip of any flaws embedded within the structure.

Residual stress data for the reactor vessel support beams have not been located. Estimates of the potential effect of residual stresses were therefore made using the following ground rules to define the

residual stress magnitude:

1. The residual stress shall not exceed the yield stress of A36 at the assumed 1100°F postweld stress relief temperature. (The yield stress at 1100°F was obtained from Ref. 7 as 16.3 ksi.) Creep-induced relaxation of the residual stresses will not be considered because no data are available on the temperature-time history used in any postweld stress relief operations. (Data reported in Ref. 22 indicate that residual stress levels approaching 10 ksi can be achieved using conventional postweld stress relief cycles.)

2. The maximum stress existing at any point within the beam, because of the sum of (a) residual stresses and (b) stresses produced by external loading, shall not exceed the material yield stress. This ground rule reflects the fact that the residual stresses are strain controlled and therefore self-limiting. A reduction of the maximum residual stress will occur if local stresses exceed the yield stress.

3. The material yield stress as defined in (2) above shall be based on the initial "as-built" yield stress of 40 ksi and include yield stress increments as a result of (a) prototypic straining rates and (b) irradiation hardening.

Estimates of the distribution of residual stress are discussed in Sect. 6.8.7.4.

6.8.7.2 Material yield stress. The material yield stress is influenced by both straining rate and irradiation hardening. Prototypic straining rates produce a 9-ksi increase in yield stress of A36 at 90°F (see Table 6.4). Irradiation-induced increases in yield stress for the HFIR material (A212B) are defined in Ref. 22 as follows:

$$\Delta\sigma_y = \Delta T/C, \quad (13)$$

where

$\Delta\sigma_y$ = incremental increase in yield stress (MPa),

ΔT = NDTT temperature shift (°C),

C = constant = 0.6.

The evaluation of potential residual stress effect was limited to the beam flange hole location, where residual stresses can be introduced by the flame-cutting operation. Irradiation-induced NDTT shifts for the location are given in Table 6.7 as 16.7°C (30°F) and 41.7°C (75°F) for the current condition and the 32-EFPY condition, respectively. Substituting these temperature shifts and constant C into Eq. (13), and converting the yield stress increments to kips per square inch, gives the following results:

Current condition: $\Delta\sigma_y = 4.0$ ksi

32 EFPY: $\Delta\sigma_y = 10.1$ ksi

In summary, the yield stress values to be considered in the analysis are as follows:

Plant operating period (EFPY)	Irradiation σ_y increment (ksi)	Static yield stress (ksi)	Dynamic yield stress (ksi)
Zero	0	40.0	49.0
7.48 (current condition)	4.0	44.0	53.0
32.0	10.1	50.1	59.1

To ensure a conservative result, in this table the assumption was made that strain-rate and irradiation effects on yield stress are directly additive.

6.8.7.3 Upper bound residual stress estimate. A finite-element analysis using the Microsafe 2D computer program⁶ was performed to define the distribution of stresses in the beam flange ligament adjacent to the grout hole. Results from this analysis are summarized in Table 6.21 and plotted in Fig. 6.53. Peak stresses from these plots were used

Fig. 6.53. Stress distributions in the beam-flange ligament showing stress concentrations adjacent to the grout hole. Tensile residual stresses can be superposed on stresses shown in HAZ section of ligament.

Table 6.21

BEAM FLANGE GROUT HOLE LIGAMENT STRESSES

Distance from hole surface		0.00	0.25	0.63	1.38	2.38	6.00
Load Combination	Max. Load (Kips.)	-----Ligament Stresses-----					
DW+TH+SBLOCA	1558.00	48903.00	39906.00	31350.00	23063.00	20049.00	16199.00
DW+TH+SSE	1212.00	38042.64	31043.69	24387.80	17941.18	15596.53	12601.53
DW+TH+OBE	1099.00	34495.76	28149.35	22114.02	16268.44	14142.39	11426.64
DW+TH	818.00	25675.64	20951.93	16459.76	12108.82	10526.37	8504.99

in conjunction with the yield stress result from Sect. 6.8.7.2 to define the limiting value of residual stress in the HAZ adjacent to the hole surface.

The first step in defining the limiting residual stress was to define the maximum residual stress that could exist at the start of life, when the material yield stress has its lowest value. This start of life limiting residual stress is the lower of (1) the material yield stress at the stress relief temperature (16.3 ksi) or (2) the static yield stress minus the stress produced by the vessel DW and T loading. This latter value is 14.3 ksi ($40.0 - 25.7$) and is therefore the governing value. In effect, the maximum residual stress throughout life cannot exceed this start-of-life value, which is governed by local yielding as the DW- and T-induced stresses (not self-limiting) are superposed on the residual stresses from the flame-cutting operation (strain controlled and self-limiting).

At the start of life, application of any static support load in excess of the DW + T load (818 kips) would cause further yielding and a further reduction in the residual stress. The yield stress is, however, increased by both high strain rates and irradiation hardening. The net result of these hardening mechanisms is to enable the material adjacent to the hole surface to sustain higher stresses without yielding. The residual stress element of the total stress, however, cannot exceed the 14.3-ksi value derived earlier. The residual stress may be reduced below the 14.3-ksi value if the combination of non-self-limiting and self-limiting stresses exceeds the dynamic yield stress of the irradiation-hardened material.

Analysis of the maximum possible residual stress associated with the three principal dynamic loading cases considered is summarized in Table 6.22. Note that the start-of-life limiting value (14.3 ksi) governs through to the end of life for the DW + T + OBE and DW + T + SSE loading cases. In the case of the DW + T + SBLOCA loading, however, the maximum residual stress is reduced by local yielding to 4.1 ksi at 7.48 EFPY and 10.2 ksi at 32.0 EFPY. The trend of increasing upper bound residual stress with increasing reactor operating period is the effect of irradiation hardening of the material.

Analysis results from Table 6.22 are plotted in Fig. 6.54. Two limiting residual stress envelopes are shown in the figure. The envelope defined by points A, B, C, and D applies at 7.48 EFPY (the current condition), while envelope A-B-E-F-C-D applies at 32 EFPY. Crack-tip, stress-intensity factors will be calculated for points E and F on the 32-EFPY envelope to define the limiting residual stress impact.

6.8.7.4 Residual stress distribution. Rapid cooling of the flame-cut surface is to be anticipated because of the highly localized heat input that is characteristic of the flame-cutting process and the "chill-block" quenching effect of the bulk of the flange plate material. This combination of thermal conditions will produce high localized tensile residual strains in the region immediately adjacent to the flame-cut surface and much lower compressive strains in the balance of the beam flange ligament. The objective in this section of the evaluation is to determine a distribution of residual stresses, compatible with the residual strain distribution, that can be used in the residual-stress/flaw-tolerance impact assessment.

Table 6.22

BEAM FLANGE GROUT HOLE LIGAMENT LIMITING RESIDUAL STRESSES

Load Combination	DW+TH	DW+TH+OBE	DW+TH+SSE	DW+TH+SB:LOCA
Max. Surface Stress = S_s (lb/in ²)	25675.64	34495.76	38042.64	48903.00
Static Yield Stress = S_y (lb/in ²)	40000.00	N.A.	N.A.	N.A.
Static Load Limited $S_r = S_{r1} = S_y - S_s$	14324.36	N.A.	N.A.	N.A.
Stress Relief Limited $S_r = S_y(1100 \text{ Deg.F})$	16300.00	N.A.	N.A.	N.A.
Limiting residual Stress At $t=Zero$: S_{rs}	14324.36	14324.36	14324.36	14324.36
Dynamic Yield At 7.48EFY = S_{yd1}	53000.00	53000.00	53000.00	53000.00
Dynamic Load Limited $S_r = S_{r2} = S_{yd1} - S_s$	27324.36	18504.24	14957.36	4097.00
Dynamic Yield At 32EFY = S_{yd2}	59100.00	59100.00	59100.00	59100.00
Dynamic Load Limited $S_r = S_{r3} = S_{yd2} - S_s$	33424.36	24604.24	21057.36	10197.00
LIMITING RESIDUAL STRESS AT 7.48 EFY	14324.36	14324.36	14324.36	4097.00
LIMITING RESIDUAL STRESS AT 32 EFY	14324.36	14324.36	14324.36	10197.00

Fig. 6.54. Envelope of residual stresses used in residual stress impact assessment. This envelope was derived using short-term yield stress of A36 as limiting condition.

No actual data on residual stresses generated by the flame-cutting process have been located. The HAZ produced by flame-cutting heavy steel plates has, however, been investigated. Results reported in Ref. 23 indicate a HAZ extending 3/16 in. into the material from the flame-cut surface. In this analysis the tensile residual stresses defined in Sect. 6.8.7.3 will be assumed to be constant over the HAZ depth. For convenience, the HAZ depth is rounded off to 0.2 in. The resulting distribution of residual stresses is given in Fig. 6.55.

Note that data reported in Ref. 23 also show a significant reduction in the NDTT in the region immediately adjacent to the flame-cut hole. This is attributed to the fine-grain structure produced by the "chill-block" quenching of the ASTM A533-B1 material used in the evaluation of Ref. 23. No comparable data have been located for A36, and it is therefore not possible to include an evaluation of quenching-induced microstructure refinements in the current analysis.

6.8.7.5 Residual stress-stress-intensity factors equations. Equations for the calculation of stress-intensity factors in clad pressure vessel walls, as a result of weld shrinkage in the cladding, are given in Ref. 24. These equations were adapted to the analysis of residual stresses adjacent to the flame-cut hole by setting the HAZ

Fig. 6.55. Normalized distribution of strain-controlled residual stresses in beam-flange grout-hole ligament based on analysis of photomicrograph data taken from sections of flame-cut, 12-in.-thick ASTM A533-B1 plate.

thickness equal to the thickness of a pseudo cladding layer. The equations taken from Ref. 24 are reproduced below.

$$K_I = \sigma^* \sqrt{\pi L \alpha} (i_0 - q \alpha i_1), \quad (14)$$

where i_0 and i_1 are the following influence functions:

$$i_0 = 1.122 + 0.9513\alpha - 0.6240\alpha^2 + 8.3306\alpha^3, \quad (15)$$

$$i_1 = 0.6825 + 0.3704\alpha - 0.0832\alpha^2 + 2.8251\alpha^3. \quad (16)$$

For deeper cracks with the crack tip beyond the HAZ, the solution is

given by

$$\begin{aligned}
 K_I = \sigma^* \sqrt{\pi L \alpha} \left(\frac{2}{\pi - 2} \right) & \left\{ \left(i_o - \frac{2}{\pi} \right) \left[(1 + R) \sin^{-1} \left(\frac{\psi}{\alpha} \right) \right. \right. \\
 & + \alpha \left[(q + Rp) \sqrt{1 - \left(\frac{\psi}{\alpha} \right)^2} - q \right] - \frac{\pi}{2} R \left. \right] \\
 & + (i_o - 1) \left[\left[(1 + R) - \frac{\psi}{2} (q + Rp) \right] \sqrt{1 - \left(\frac{\psi}{2} \right)^2} \right. \\
 & \left. \left. + \frac{\alpha}{2} (q + Rp) \sin^{-1} \left(\frac{\psi}{\alpha} \right) - 1 - \frac{\pi}{4} Rp \alpha \right] \right\} . \quad (17)
 \end{aligned}$$

In these equations, parameters p and q define the slopes of linear fits to the residual stress distributions. In the assumed residual stress distribution of Fig. 6.55, these slopes are zero, resulting in a considerable simplification of Eqs. (14) and (17). The remaining terms in these equations, as adapted for the HAZ residual stress analysis, are defined as follows:

- x = distance from the flame-cut surface (in.)
- L = flange ligament width = 6.0 in.
- a = flaw depth (in.)
- α = flaw depth/ligament width ratio = a/L
- ψ = HAZ thickness/ligament width = $0.2/6 = 0.0333$
- σ^* = HAZ residual tensile stress (lb/in.²)
- p, q = stress distribution slope constants = 0

$$R = \frac{\text{HAZ thickness}}{(\text{Ligament width} - \text{HAZ thickness})} = \frac{0.2}{(6.0 - 0.2)} = 0.0345$$

The above definition of R applies only to the simplified residual stress distribution shown in Fig. 6.55.

6.8.7.6 Ligament combined stress-intensity factors. Linear superposition was used to combine the residual-stress, stress-intensity factors defined in Sect. 6.8.7.5 with those generated by loads applied to the supports. Stress-intensity factors generated by external loading were taken from the analysis of Sect. 6.7.3. Support loads and residual stresses used in the analysis (corresponding to points E and F on Fig.

6.54) were defined as follows:

Point on Fig. 6.54	Physical significance	Support load (kips)	Residual stress (lb/in. ³)
E	Highest support load for which the maximum residual stress can be sustained	1427	14,300
F	Maximum support load currently specified for the plant	1558	10,200

Ligament stresses corresponding to points E and F in Fig. 6.54 are summarized in Table 6.23 and shown plotted in Fig. 6.56 and 6.57.

Stress-intensity factors corresponding to the applied loading stress, the residual stress, and the combined stress are calculated in the spreadsheets of Tables 6.24 and 6.25 for cases "E" and "F" respectively. The analysis was performed parametrically with the flaw depth "a" as an independent variable. Plots of the stress-intensity factors obtained for the two cases are given in Figs. 6.58 and 6.59.

Superposed on the plots of Fig. 6.58 and 6.59 is the 32-EFPY K_{Id} value for the beam flange hole location (48.6 ksi·√in.). The intercept of the K_{Id} line with the K_I curves for (1) support loading acting alone and (2) support loading combined with residual stresses provides an indication of the potential effect of the residual stresses. In case "E" (Fig. 6.58), residual stresses reduce the critical flaw depth to a combined loading value of just under 0.2 in. Case "F" results show a smaller critical flaw size reduction (0.21 in.) to a combined loading value of just over 0.2 in. The impact of incorporating the effect of the upper bound residual stresses is therefore to reduce the critical flaw depth at the beam flange hole location to 0.2 in.

6.8.7.7 Assessment of results. Inclusion of residual stress effects in the flaw tolerance analysis of the beam flange hole reduces the 32-EFPY critical flaw size to ~50% of the value calculated when residual stress effects were ignored. Several critical assumptions had to be made in the analysis that produced this result, and these could have a significant impact on the calculated flaw size reduction. There appears to be a potential for significant reductions in critical flaw sizes because of residual stress effects, however, and recognition of this potential should be factored into any evaluation of the overall analysis results.

Refinement of the microstructure adjacent to flame-cut holes in heavy-section material has been shown to have the potential for producing beneficial shifts in the NDTT of ASTM A533-B1 material. It is possible that similar beneficial microstructure changes may occur adjacent to flame-cut holes in A36 material. No data on flame-cutting-induced microstructure refinement in A36 material have been located, however, and therefore it was not possible to include consideration of this effect in the flaw tolerance analysis.

Table 6.23

COMBINED BEAM FLANGE LIGAMENT STRESSES

Distance from hole surface	0.00	0.20	0.20	0.25	0.63	1.38	2.38	6.00
DW+TH+SBLOCA (1558 Kips)	48903.00	41705.40	41705.40	39906.00	31350.00	23063.00	20049.00	16199.00
10200 PSI Residual Stress	10200.00	10200.00	-351.72	-351.72	-351.72	-351.72	-351.72	-351.72
TOTAL CASE "F"	59103.00	51905.40	41353.68	39554.28	30998.28	22711.28	19697.28	15847.28
1427 Kips Applied Support Load	44791.13	38198.72	38198.72	36550.62	28714.02	21123.81	18363.24	14836.95
14300 PSI Residual Stress	14300.00	14300.00	-493.10	-493.10	-493.10	-493.10	-493.10	-493.10
TOTAL CASE "E"	59091.13	52498.72	37705.62	36057.52	28220.93	20630.72	17870.14	14343.86

Fig. 6.56. Combination of primary stresses and residual stresses used for flaw tolerance analysis of point E on residual stress envelope.

Fig. 6.57. Superposition of residual stresses and stresses produced by DW + T + SBLOCA loading condition. This stress distribution was used in evaluation of point F on residual stress envelope.

Table 6.24

IMPACT OF RESIDUAL STRESS ON CRITICAL FLAW SIZES IN THE BEAM FLANGE GROUT HOLE LIGAMENT : CASE "E"

Stress location :	4 in. diameter hole in the upper beam flange: inner concrete not effective								
Flaw type	Uniform depth radial flaw								
Hole diameter in.	4.000								
Plate width in.	16.000								
Stresses in beam flange without flaw :	Unit load (200 Kip) BM	Load case maximum load	Load case maximum BM	Max. surface stress	Flange mean stress:Note 1	Flange bend. stress			
Support load for max. residual stress (1427 Kips.)	1493000.000	1427000.000	10652555.000	17471.479	15044.885	2426.594			
Flaw depth "a" in.	0.050	0.100	0.150	0.200	0.300	0.400	0.600	1.000	
Flaw depth to flange width ratio (R+a)/W	0.256	0.263	0.269	0.275	0.288	0.300	0.325	0.375	
Membrane correction factor = F (Ref. 24)	0.520	0.730	0.840	0.910	1.010	1.065	1.150	1.200	
Faulted condition (2)									
$K1 = S_m * F * \sqrt{PI} * \sqrt{R+a}$	19853.820	28209.560	32844.475	35992.875	40845.959	43996.592	49447.953	55424.997	
RESIDUAL STRESS IMPACT ASSESSMENT									
Residual stress (KSI.) = S_r	14300.000								
Flange ligament width	6.000								
Influence function I_o : Equation (15)	1.130	1.138	1.146	1.153	1.169	1.185	1.219	1.302	
Flaw within HAZ : Equation (14) : Res. Str. SIF	6403.711	9118.984	11245.021	13073.075					
Flaw extends beyond HAZ : Equation (17):Res Str SIF					9074.359	7509.405	5959.249	4552.019	
Combined stress intensity factor	26257.531	37328.544	44089.496	49065.950	49920.318	51505.997	55407.202	59977.017	

Notes : (1) This stress is based on the full flange width in order to match the uniform far field stress boundary condition incorporated into the stress intensity factor equation. (The flange hole has not been taken out)

Table 6.25

IMPACT OF RESIDUAL STRESS ON CRITICAL FLAW SIZES IN THE BEAM FLANGE GROUT HOLE LIGAMENT : CASE "F"

Stress location :	4 in. diameter hole in the upper beam flange: inner concrete not effective								
Flaw type	Uniform depth radial flaw								
Hole diameter in.	4.000								
Plate width in.	16.000								
Stresses in beam flange without flaw :	Unit load (200 Kip) BM	Load case maximum load	Load case maximum BM	Max. surface stress	Flange mean stress:Note 1	Flange bend. stress			
DW + T + SB:LOCA support load	1493000.000	1558000.000	11630470.000	19075.378	16426.020	2649.358			
Flaw depth "a" in.	0.050	0.100	0.150	0.200	0.300	0.400	0.600	1.000	
Flaw depth to flange width ratio (R+a)/W	0.256	0.263	0.269	0.275	0.288	0.300	0.325	0.375	
Membrane correction factor = F (Ref. 24)	0.520	0.730	0.840	0.910	1.010	1.065	1.150	1.200	
Faulted condition (2)									
$K_I = S_m \cdot F \cdot \sqrt{\pi a} \cdot \sqrt{R+a}$	21676.420	30799.225	35859.630	39297.056	44595.658	48035.522	53987.324	60513.066	
RESIDUAL STRESS IMPACT ASSESSMENT									
Residual stress (KSI.) = S_r	10100.000								
Flange ligament width	6.000								
Influence function I_o : Equation (15)	1.130	1.138	1.146	1.153	1.169	1.185	1.219	1.302	
Flaw within HAZ : Equation (14) : Res. Str. SIF	4522.901	6440.681	7942.288	9233.431					
Flaw extends beyond HAZ : Equation (17):Res Str SIF					6409.164	5303.848	4208.982	3215.066	
Combined stress intensity factor	26199.321	37239.906	43801.918	48530.487	51004.822	53339.369	58196.306	63728.132	

Notes : (1) This stress is based on the full flange width in order to match the uniform far field stress boundary condition incorporated into the stress intensity factor equation. (The flange hole has not been taken out)

Fig. 6.58. Stress-intensity factors produced by combination of primary stresses and residual stresses at point E on beam-flange grout-hole residual stress envelope. Addition of residual stresses reduces critical flaw size by 0.38 to 0.2 in.

Fig. 6.59. Stress-intensity factors produced by combination of primary stresses and residual stresses at point F on beam-flange grout-hole residual stress envelope. Addition of residual stresses reduces critical flaw size by 0.21 in.

The generation of data on the possible beneficial effects of chill-quenching-induced microstructure refinement is recommended as an integral part of any further evaluation of the flame-cut grout hole. Potential sources for these data are identical with those identified in Sect. 6.8.4.

6.9 Conclusions and Recommendations

Accelerated irradiation-induced embrittlement of the Trojan reactor vessel support beams is predicted to occur to the point where brittle fracture becomes a credible end-of-life (32-EFPY) failure mode.

Critical flaws calculated for regions of the A36 support beams, remote from structural discontinuities, have a configuration and dimensions that would make them relatively easy to detect using surface inspection techniques (visual, dye penetrant, and magnetic particle). The contribution of fatigue flaw growth to the dimension of the critical flaw is negligible. For the critical flaws to be present in the installed support beams, they would have had to be present at the time of final inspection of the support beam assembly. It is considered unlikely therefore that a brittle fracture potential exists in those portions of the support beams remote from structural discontinuities, because flaws of the required size would have been readily detected and eliminated during the fabrication process. Bases for this conclusion should be verified by a review of the inspection procedures and any available inspection records.

Flame-cut, 4-in.-diam holes exist in the beam flanges, immediately above the inner support pedestals. Stress concentrations produced by these features reduce the end-of-life critical depth of radial flaws emanating from the hole to 0.42 in. for the SBLOCA loading condition. Residual stresses induced by the flame-cutting process could further reduce this critical flaw depth. It is considered unlikely that flaws of this magnitude could have been reliably detected in any inspection of the flame-cut surface of the hole. Recommendations are given for actions that could provide a generic definition of (1) the distribution of flaws to be anticipated adjacent to the flame-cut hole and (2) any beneficial effects produced as a result of chill-quenching-induced refinement of the material microstructure adjacent to the flame-cut surface. Further evaluation of this feature of the support will be required when these data become available.

Extrapolation of the available fracture-toughness data base for A36 was required to cover operating conditions for critical portions of the beam embedded within the concrete. Generation of fracture-toughness data for these conditions is recommended.

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7. BRITTLE FRACTURE EVALUATION OF TURKEY POINT UNIT 3 REACTOR PRESSURE VESSEL SUPPORTS

7.1 Introduction and Summary

As indicated in Figs. 7.1-7.6, the reactor vessel for the Turkey Point Unit 3 plant is supported on steel cantilever-beam arrays that are positioned under each of the coolant nozzles and that extend from this position into the concrete biological shield. The support load at each nozzle is transmitted from the nozzle to the cantilever-beam array through a set of rollers, lateral restraints, and a steel girder that is bolted to and joins together the cantilever beams in an array. Three components of this assembly were selected for evaluation with regard to propagation of flaws: the cantilever beam, the bolts that join the

Fig. 7.1. Reactor vessel supports plan view.

ORNL-DWG 88-5062 ETD

Fig. 7.2. Elevation view of reactor vessel support system.

ORNL-DWG 88-5018 ETD

Fig. 7.3. Symmetric support-array plan view.

ORNL-DWG 88-5019 ETD

Fig. 7.4. Asymmetric support-array plan view.

Fig. 7.5. Section elevation of symmetric array.

Fig. 7.6. Elevation of symmetric array.

girder and beams, and the lateral restraints. The cantilever beam is considered to be the more critical of the three, and thus the emphasis in this report is on the beam.

The beam is fabricated from A588, a low-alloy structural steel with a minimum yield strength of 50,000 psi. The radiation sensitivity of A588 was assumed to be essentially the same as for the HFIR pressure vessel materials, and the radiation-damage trend curves deduced from the HFIR vessel surveillance data and discussed in Chap. 3 were used for this evaluation. Best-estimate present-day and 32-EFPY values of $\Delta NDTT$ corresponding to the location of the minimum critical flaw size are 30 and 55°F, respectively.

Essentially no fracture-toughness data, other than Charpy V-notch (CVN) data, are available for A588, and thus the ASME Sects. III and XI K_{Ic} and K_{IR} curves were used. Dynamic response data were not available for the Turkey Point vessel support system, and thus K_{IR} was used as the best estimate of the appropriate K_{Id} .

Loads applied to the supports were supplied by the utility and included those for the large-break loss-of-coolant accident (abbreviated as LOCA in this chapter of the report), operating basis earthquake (OBE), safe shutdown earthquake (SSE), and deadweight or dead load (DL). It is ORNL's understanding that Turkey Point still must consider the LOCA credible, and thus it was treated as such in this study.* (The utility did not provide loads for a small-break loss-of-coolant accident, and as mentioned previously, no dynamic loading data were furnished.)

Significant uncertainties exist in many of the parameters included in the calculation of the critical flaw size. Thus, the study was conducted in a parametric manner so that ranges of critical flaw sizes were obtained. Results for the beam (Figs. 7.7-7.12) indicate that for the most severe credible loading (DL + LOCA) and 32 EFPY, the best-estimate minimum critical flaw size (depth) is 0.3 in. The flaw size is insensitive to reactor operating time after ~10 EFPY, and at plant startup (0 EFPY) the size is 0.6 in. The flaw is a corner crack in the upper flange anywhere between the inner surface of the biological shield and a point ~4 in. into the shield. Considering the uncertainties in the fracture toughness, initial NDTT, and the operating temperature of the beam, the $\pm 1\sigma$ (one standard deviation) values of the critical flaw size at 32 EFPY are ~0.2 and 0.6 in.

For the DL + SSE condition, the critical flaw size is substantially larger (1.1 in. compared with 0.3 in. for best-estimate values and 32 EFPY).

7.2 Description of Supports

The Turkey Point reactor vessel is supported on cantilever-beam arrays positioned under each of the coolant nozzles (Figs. 7.1-7.6). A

*Many PWR plants have been exempt from consideration of large-break loss-of-coolant accident loads, but at the time of this study, Turkey Point was not.

Fig. 7.7. Critical size of corner flaw in cantilever beam vs service life and fracture-toughness curve (mean value of $T - NDTT$, $L = 17$ -in. position, DL + LOCA loading, Method B $\Delta NDTT$).

Fig. 7.8. Critical size of corner flaw in cantilever beam vs service life and $T - NDTT$ Gaussian position ($L = 17$ -in. position, DL + LOCA loading, Method B $\Delta NDTT$, ASME K_{IR}).

Fig. 7.9. Critical size of corner flaw in cantilever beam vs service life and T - NDTT Gaussian position (L = 17-in. position, DL + LOCA loading, Method B Δ NDTT, ASME K_{IC}).

Fig. 7.10. Critical size of corner flaw in cantilever beam vs service life and fracture-toughness curve (L = 17-in. position, DL + SSE loading, Method B Δ NDTT).

Fig. 7.11. Critical size of corner flaw in cantilever beam vs service life and T - NDTT Gaussian position (L = 17-in. position, DL + SSE loading, Method B Δ NDTT, ASME K_{IR}).

Fig. 7.12. Critical size of corner flaw in cantilever beam vs service life and T - NDTT Gaussian position (L = 17-in. position, DL + SSE loading, Method B Δ NDTT, ASME K_{IC} curve).

nested group of three rollers transmits vertical load from the pad under each nozzle to a wide-flange girder and also accommodates radial thermal expansion of the vessel. Each girder rests on three wide-flange beams that project inward from the biological shield. Welded to each girder are lateral restraints that project upward on either side of the nozzle pads to transfer horizontal loads to the cantilever beams. Each cantilever beam is welded to two supporting columns that are embedded within the biological shield. The column base plates were anchored to the biological shield at an intermediate stage of concrete pouring. Each column has an array of "Nelson" studs to provide for load transference from the structural steel members to the reinforced concrete biological shield. Two beam arrays are symmetrically oriented with respect to the vessel nozzle centerlines; four beam arrays are asymmetrically located. The two types of support arrays were designed to have equivalent compliances to vessel loading.

The dimensions $L = 13$ in. and $L = 17$ in. (Figs. 7.3 and 7.4) define the locations of flaw assessment for the cantilever beams. They are the distances from the point of load application to the interfacing of the beam with, respectively, the biological shield and the nearest support column. The location defined by $L = 13$ in. is the point of maximum radiation damage and significant stress in the portion of the beam within the cavity; the location defined by $L = 17$ in. is the point of maximum stress in the portion of the beam within the biological shield.

7.3 Materials

The principal vessel support components of interest and the corresponding materials for Turkey Point are as follows:

<u>Component</u>	<u>Material</u>
Cantilever beams	Bethlehem Steel Co. proprietary steel Mayari R-50 conforming to American Society for Testing and Materials (ASTM) A588
Girder-to-cantilever beam bolts	ASTM A354-BC
Lateral restraints	ASTM A302-B

7.4 Material Fracture-Toughness Properties

No mechanical or fracture-toughness properties are available for the heats used in fabrication of the supports, and no data are available in the open literature for ASTM A354-BC. However, a few dynamic data (unknown loading rate) are available for A588¹ and are compared with the ASME K_{IC} and K_{IR} curves² in Fig. 7.13 (the data as published are not

Fig. 7.13. Estimated K_{Id} values for A588 steel from βK_C -corrected K_C data compared with ASME K_{IC} and K_{IR} curves.

valid in accordance with ASTM E399,³ but those shown in Fig. 7.13 have been converted to "valid" values using a method proposed by Merkle⁴). Except for one data point, the K_{IR} curve, appears to represent the dynamic data adequately. In the absence of more definitive data, ORNL chose to evaluate the cantilever beams premised on the ASME K_{IR} curve. Because ASTM A302-B fracture-toughness data are an integral basis for the K_{IR} curve, evaluation of the lateral restraints logically should use this data base also. The evaluation of girder-to-cantilever beam bolts has also employed the K_{IR} curve.

Although the K_{IR} curve was selected for evaluation of the Turkey Point vessel supports, it was also of interest to perform some calculations using the ASME K_{IC} curve, which represents the lower bound of static crack initiation data for a select group of pressure vessel low-alloy carbon steels.

As previously noted, no specific data on the initial nil-ductility transition temperature ($NDTT_0$) are available for the components assessed. However, Ref. 1 includes CVN data for A588 and indicates that for this material $NDTT_0 = T_{30}$, the temperature at which CVN = 30 ft·lb. Based on this approach and the included CVN data (Fig. G-5 of Ref. 1), $NDTT_0 \approx 35^\circ\text{F}$.

An inquiry was made to Bethlehem Steel on fracture toughness and $NDTT$ of Mayari R50 steel. In response,⁵ it was recommended that data on ASTM A572, provided in an accompanying paper,⁶ would represent a "worst-case" condition for large rolled-section steels. Assuming $NDTT_0 = T_{30}$ to be appropriate, Fig. 7 of Ref. 6 indicates a range in $NDTT_0$ of 10 to 95°F . In earlier studies^{7,8} conducted at Lehigh University, in a few instances, the use of T_{30} would infer $NDTT_0$ s exceeding 150°F .

Reference 9 includes a statistical analysis of several heats of A572 steel that indicates mean values for $NDTT_0$ of 25°F for the as-hot-rolled condition and -50°F for the normalized condition. A review of Table B.8 of Ref. 9 indicates that data for a thickness range of 0.625 to 2.5 in. were used for the as-hot-rolled condition and that data for a range of 0.5 to 1.5 in. were used for the normalized condition. Furthermore, when restricted to a range of 1 to 1.5 in., the $NDTT_0$ for the as-hot-rolled condition exhibited a range from 20 to 50°F. The pooled $NDTT_0$ data on A572 from Ref. 9 (Fig. B13) exhibited a range from -60 to 100°F. The low-temperature values in this range appear to represent data taken from thin, normalized material, while the higher values correspond to the as-hot-rolled thicker sections.

The data of Refs. 10 and 11 indicate that the $NDTT_0$ of A588 material and associated heat-affected zone (HAZ) and weld may be raised by as much as 40°F as a result of a postweld heat treatment.

It is not known whether the cantilever beam was normalized and/or subjected to a postweld heat treatment. In the absence of this information but considering the data that are available (Refs. 5-11) and the thickness of the beam flanges (2.5 in.), ORNL selected $NDTT_0 = 65 \pm 65^\circ\text{F}$. No definitive data for the A354-BC bolting material could be found, and thus $NDTT_0 = 65 \pm 65^\circ\text{F}$ was selected for the bolts also.

Munse¹² estimates that a typical range for $NDTT_0$ for 2-1/2 to 4-1/2-in.-thick steel material conforming to ASTM A302-B would be 10 to 100°F. Thus, $NDTT_0 = 55 \pm 45^\circ\text{F}$ was selected for the evaluation of the lateral restraints.

As discussed in Chap. 3, the radiation-damage trend curve ($\Delta NDTT$ vs dpa, dpa/s) was assumed to be the same for the support materials as for the HFIR vessel materials, and it was necessary to extrapolate the HFIR data relative to dpa and dpa rate to apply it to the supports. Two methods of extrapolation (Method A and B, as discussed in Chap. 3) have been used in this evaluation. Figure 7.14 applies to Method A, and Fig. 7.15 applies to Method B. As discussed in Chap. 3, Method B is perhaps the preferable procedure for obtaining $\Delta NDTT$. Nevertheless, because of the considerable uncertainty as to the best method for extrapolation of HFIR data, it was considered prudent to incorporate both methods in the evaluation of the Turkey Point reactor vessel supports.

As indicated in Fig. 7.13, the ASME K_{IC} and K_{IR} values are plotted as a function of $T - RT_{NDT}$, where RT_{NDT} is the reference nil-ductility temperature. It was assumed for this study that $RT_{NDT} = NDTT = NDTT_0 + \Delta NDTT$. Each of the terms in the expression $(T - NDTT_0 - \Delta NDTT)$ has considerable uncertainty. To account for these uncertainties in a simplistic manner for this initial study, it was assumed that the expression has a Gaussian distribution and that the algebraic sums of the uncertainty limits for T and $NDTT_0$ correspond to the $\pm 3\sigma$ position of the Gaussian distribution for $T - NDTT$ (no uncertainty is assigned to $\Delta NDTT$). The fracture toughness (K_{IC} and K_{IR}) was evaluated at the following values of the presumed $T - NDTT$ Gaussian distribution: -3σ , -1σ , mean, $+1\sigma$, and $+3\sigma$. The 1σ values have been determined as follows:

$$(T - NDTT)_{1\sigma} = \pm \frac{0.6827}{0.9973} [(T - NDTT)_{\max} - (T - NDTT)_{\min}] / 2 ,$$

Fig. 7.14. Method A extrapolation of HFIR ANDTT vs dpa, dpa/s data.

Fig. 7.15. Method B extrapolation of HFIR ANDTT vs dpa, dpa/s data.

where "max" and "min" refer to the uncertainty limits. Thus, the $T - \text{NDTT}$ values evaluated as functions of the Gaussian distribution are as follows:

Gaussian point	Evaluation expression
-3σ	$(T - \text{NDTT})_{\min}$
-1σ	$(T - \text{NDTT})_{\text{mean}} - 0.6845 [(T - \text{NDTT})_{\min} - \text{mean}]$
Mean	$0.5 [(T - \text{NDTT})_{\max} + (T - \text{NDTT})_{\min}]$
$+1\sigma$	$(T - \text{NDTT})_{\text{mean}} + 0.6845 [(T - \text{NDTT})_{\max} - \text{mean}]$
3σ	$(T - \text{NDTT})_{\max}$

7.5 Thermal and Radiation Environment

The range of operating temperatures T for the Turkey Point reactor vessel support components is not known at this time. The maximum operating temperature for the containment is understood to be 120°F . Figure V of Ref. 13 provides the maximum anticipated temperatures that will exist across the biological shield. At the $L = 13\text{-in.}$ position (interface with the biological shield), the inferred maximum temperature for the cantilever beam is 130°F . At the $L = 17\text{-in.}$ position, the point of highest vertical and horizontal bending stress, the inferred maximum temperature is 138°F . Information received from Westinghouse Electric Corporation on the temperature of the shoe of the Trojan Plant reactor vessel support (see Chap. 6) infers that the lateral restraints of the Turkey Point reactor vessel supports will operate at a temperature of $\sim 340^{\circ}\text{F}$ at the point of maximum bending stress.

ORNL has assumed a 30°F operating temperature range for all components of the reactor vessel support other than the lateral restraint. A 10°F operating range has been assumed for this latter component. Consequently, the operating temperature ranges are as follow:

Component	Temperature range ($^{\circ}\text{F}$)
Cantilever beam	
$L = 13\text{-in.}$ position	100 to 130
$L = 17\text{-in.}$ position	108 to 138
Girder-to-cantilever beam bolt	90 to 120
Lateral restraint	330 to 340

Extensive sophisticated calculations of neutron flux and dpa rate were performed by Florida Power and Light Co.^{14,15} for the Turkey Point vessel supports. Figure 7.16 shows the location of ten points on the

Fig. 7.16. Location for which neutron fluxes calculated.

supports for which neutron flux and dpa rate were calculated. Points 4, 8, 9, and 10 correspond to, respectively, (1) the girder-to-cantilever bolts, (2) lateral restraints, (3) cantilever beams at loading arm length $L = 13$ in. (interface with biological shield surface), and (4) cantilever beams at loading arm length $L = 17$ in. (intersection with support column and location of maximum bending stress).

Table 7.1 summarizes the dpa-rate data for the ten points as a function of time, that is, fuel cycles (the dpa rate for point 10 was estimated by multiplying the dpa rates at point 9 by the ratio of fluxes at points 10 and 9 given in Fig. 7.17). The data in Table 7.1 were adjusted by time weighting to provide an estimate of the mean dpa rate and dpa from initial plant startup until the end of the eighth fuel cycle (7.04 EFPY, Oct. 1, 1983), ninth fuel cycle (11.79 EFPY, Aug. 3, 1988), and 32 EFPY for points 4, 8, 9, and 10. These data are provided in Table 7.2.

Table 7.1. Calculated dpa rates around the
Turkey Point reactor vessel supports

Data point No.	dpa/s (before October 1, 1983) ^a		dpa/s (beginning with January 7, 1984)	
	E ≥ 1.0 MeV	E ≥ 0.11 MeV	E ≥ 1.0 MeV	E ≥ 0.11 MeV
1	3.844-13 ^b	5.909-13	2.103-13	3.213-13
2	4.930-13	9.123-13	2.750-13	5.010-13
3	6.523-13	1.163-12	3.802-13	6.683-13
4	3.492-13	6.996-13	2.073-13	4.066-13
5	2.088-13	4.713-13	1.204-13	2.787-13
6	2.297-13	4.666-13	1.440-13	2.830-13
7	8.888-14	2.009-13	5.816-14	1.249-13
8	4.319-14	1.272-13	2.705-14	7.640-14
9	3.829-14	1.141-13	2.296-14	6.621-14
10		3.582-14		2.052-14

^aSteel displacement cross sections and calculation procedures for dpa/s are based on ASTM E 693-79.

^bRead 3.844-13 as 3.844×10^{-13} .

Table 7.2. Time-weighted dpa rates (E > 0.1 MeV)
and dpa as a function of fuel cycle

Data point No.	Data through Oct. 1, 1983, 8 fuel cycles 7.04 EFY Total time lapse: 2.020 + 8 s ^a		Data through Aug. 3, 1988, 7 fuel cycles 11.79 EFY Total time lapse: 3.719 + 8 s		Data through to 32 EFY Total time lapses 10.091 + 8 s	
	Mean dpa/s	dpa	Mean dpa/s	dpa	Mean dpa/s	dpa
4	6.996-13	1.553-4	5.815-13	2.163-4	4.719-13	2.302-4
8	1.272-13	2.824-5	1.067-13	3.969-5	8.773-14	8.853-5
9	1.141-13	2.533-5	9.02-14	3.525-5	7.67-14	7.744-5
10	3.582-14	7.95-6	2.965-14	1.103-5	2.393-14	2.415-5

^aRead 6.996-13 as 6.996×10^{-13} , and 2.020 + 8 s as 2.020×10^8 s.

Fig. 7.17. Fast-flux distribution in biological shield.

7.6 Estimates of Δ NDTT

The radiation-damage trend curves (Δ NDTT vs dpa and dpa rate)² used in this study for the support materials are discussed in Sects. 7.3 and 7.4, and the dpa rate and dpa data for the support are given in Tables 7.1 and 7.2. Values of Δ NDTT derived from this information are provided in Table 7.3 as a function of time and trend curve for each support component considered.

7.7 Support Loadings and Stress Analysis

Critical-flaw-size assessment of components of the Turkey Point reactor vessel supports has been premised on utilization of the design analysis performed by Bechtel Corporation, the architect-engineer (AE) for this plant. Only minor corrections or extensions in analysis to the Bechtel stress analyses have been required for the assessments as hereafter explained.

Table 7.3. Summary of estimated radiation-damage shift Δ NDTT as a function of extrapolation method and service life for each component

Component	Δ NDTT ($^{\circ}$ F)					
	7.04 EFPY		11.79 EFPY		32 EFPY	
	A ^a	B ^a	A	B	A	B
Cantilever beam Point 9, L = 13 in.	b	30	b	40	130	75
Cantilever beam Point 10, L = 17 in.	b	20	b	30	30 ^b	55
Girder-to-cantilever beam bolt - Point 4	b	45	40 ^b	65	70	75
Lateral restraint Point 8	b	30	30 ^b	40	130	80

^aHFIR data extrapolation Methods A and B.

^bExtrapolation Method A yields questionable values below a Δ NDTT shift of 50 $^{\circ}$ F.

As noted in Chap. 4, the AE analysis of PWR supports in general does not incorporate an assessment of the effect of differential friction in support provisions for radial expansion. Such an effect was not examined by the Turkey Point AE, and ORNL has not made an assessment of the potential consequences. Furthermore, residual and thermal stresses are important with regard to evaluating critical flaw sizes, but insufficient information was available for their inclusion. As a result of these omissions, it may be that actual critical flaw sizes are less than those calculated in this study.

The Bechtel design document for the Turkey Point containment structures¹³ and five sets of Bechtel calculations¹⁶⁻²⁰ in support of the design thereof were supplied by Florida Power and Light Co. Design calculations included in Refs. 16 and 17 appear to involve a conceptual stage of design, whereas Refs. 18-20 appear to represent the final design. In the conceptual stage of design, the AE designer assumed that the cantilever beams were "built-in" at the point of interface with the biological shield (identified as the L = 13-in. position on Figs. 7.3 and 7.4). In the final stage of design, the AE designer assumed that the cantilever beams were "built-in" at the inner face of the inboard column (identified in Figs. 7.3 and 7.4 with the dimension L = 17 in.). The AE designer assumed that the concrete between the biological shield inner surface and the inboard column provided no effective support to the cantilever beams. These assumptions reflect standard design practice and are consistent with the analysis performed in Chap. 6.

The evaluations of the cantilever beams and lateral restraints were performed using the loadings and stresses from Refs. 18-20. The loads and stresses for the girder-to-cantilever-beam bolts are covered only in Refs. 16 and 17 but were corrected to reflect the subsequent design changes in Refs. 18-20 that are a consequence of a required increase in girder size. In addition to this correction, the stresses that result from the load combination of DL and no-loss-of-function earthquake (E') (SSE in the vernacular of later plant designs) were also evaluated. This latter loading condition was not germane to the Bechtel design analysis because it did not control the size of the components. Therefore, it was not included in Refs. 16-20.

Note that in contrast to the Trojan Plant discussed in Chap. 6, pipe loadings resulting from thermal constraint were considered to be insignificant for Turkey Point. In addition, dynamic methods of analysis were evidently unavailable at the time of the Turkey Point design so that static analysis of enveloped loads were employed to ensure that the reactor vessel support design could adequately accommodate loadings involving LOCAs. In further contrast to Trojan, the LOCA was included by the utility as a credible loading condition for Turkey Point. Many PWR plants have been exempt from consideration of LOCA loads, but at the time of this study Turkey Point was not.

The LOCA "break" was assumed to be a multioriented, axially split pipe failure rather than a guillotine type. A significant attendant consequence of this assumption is the important role of horizontal loadings in the design analysis and the calculation of critical flaw size for the cantilever beams.

Table 7.4 provides the load combinations used by the Bechtel Corp. designer for the final design of the Turkey Point vessel supports, and Table 7.5 provides the corresponding stresses for the cantilever beams, girder-to-cantilever beam bolts, and lateral restraints. As previously noted, to permit evaluation of critical flaw size as a function of loading condition 3 (Table 7.5), ORNL calculated the stresses shown in parentheses in Table 7.5, using the stress analysis method of the AE given in Refs. 16-20.

7.8 Stress-Intensity-Factor (K_I) Evaluation

Because of the large influence that the horizontal load had on the design of the vessel-support cantilever beams, a variety of flaw shapes was assumed — some more responsive to vertical loading, some to horizontal, and one to both directions of loading. Semielliptical surface flaws with varying ratios of major to minor diameter {from 1.0 (semi-circular) to ∞ [single-edge-notch (SEN) flaw]} were assumed to exist on the top surface of the cantilever-beam flange; these flaws were particularly responsive to vertical loading. SEN flaws were assumed to exist on the edges of the top flange (these flaws were particularly responsive to horizontal loading), and quarter-circular corner flaws were assumed to exist in the upper corner of the top flange (these flaws were responsive to both vertical and horizontal loading).

Table 7.4. Summary of Turkey Point vessel and vessel support loads

Loading condition ^a	Vessel loads ^b : force (kips) moment (kips·ft)	Number of support arrays assumed reacting ^c	Load per support array (kips)
1. DL	V = 1684	3	V = 562
2. DL and E	V = 1802 H = 200	6 3	V = 300 H = 66
3. DL and E'	V = 2105 H = 623	6 3	V = 350 H = 203
4. DL + LOCA(V)	V = 3074 M = 21,000	6	V = 1418 (max)
5. DL + LOCA(H)	V = 1684 H = 900	6 1	V = 280 H = 900 (max)
6. DL + LOCA(45°)	V = 2666 H = 635 M = 14,900	5 6	V = 1084 H = 635

^aNotation used in Refs. 16-20

DL: operational dead load,

E: operating basis earthquake (OBE) = 0.3 E',

E': no-loss-of-functions earthquake [safe shutdown earthquake (SSE)],

LOCA(V): vertically acting large-break loss-of-coolant-accident load,

LOCA(H): horizontally acting (normal to nozzle axis) LOCA load,

LOCA(45°): LOCA load (normal to nozzle axis) at 45° to vertical.

^bV = vertical component,

H = horizontal component,

M = moment.

^cA support array includes three cantilever beams (see Figs. 7.3 and 7.4).

Table 7.5. Summary of stress values for components of Turkey Point reactor vessel supports as a function of loading condition

Case No.	Loading condition ^a	L ^b (in.)	Stress (σ), ^c ksi					
			Symmetric cantilever beam		Girder-to-cantilever-beam bolt	Lateral restraint	Nonsymmetric cantilever beam	
			σ_y	σ_x	σ	σ	σ_y	σ_x
1	DL	17	5.7				5.3	
2	DL + E	17	4.6 ^d	5.1 ^d				
3A	DL + E'	17	8.2 ^d	5.6 ^d	9.30 ^d	6.2		
3B		13	(6.27) ^d	(11.9) ^d				
4	DL + LOCA(V)	17	14.3					
5A	DL + LOCA(H)	17	23.2	23.0	41.22 ^d	27.4	12.0	16.3
5B		13	(17.7) ^d	(17.6) ^d				
6A	DL + LOCA(45°)	17	25.5	16.3			10.3	16.6
6B		13	(19.5) ^d	(12.5) ^d				

^aSee footnote a in Table 7.4.^bApplies to cantilever beam only,

L = 13 in. is at inner surface of shield,

L = 17 in. is at location of maximum bending moment.

^c σ = stress, y subscript bending about x-x neutral axis, x subscript bending about y-y axis.^dEvaluation by ORNL based on Bechtel Corp. design analysis.

The girder-to-cantilever beam bolts were assumed to have complete circumferential flaws oriented perpendicular to the bolt centerline and therefore were responsive to bolt tensile loading. The lateral restraints were assumed to have semielliptical surface flaws oriented perpendicular to and intersecting the vertical surfaces and therefore were responsive to horizontal loading. Aspect ratios of 1:1 to 6:1 were considered for these latter flaws.

The stress-intensity factors for semielliptical flaws in the cantilever beam and lateral restraint were evaluated by means of the method suggested by Merkle.^{21,22} (Previous comparisons²³ of this solution with finite-element analyses indicate that the stress-intensity factors for some elliptical shapes other than semicircular are overestimated. Nevertheless, its use was considered to be expedient for this study because the analytical format facilitates the parametric investigation.)

The Merkle solution was developed for a combined tension and bending-stress field and permits evaluation of K_I as a function of position on the crack profile. In this study the maximum value of K_I on the crack front was assumed to be the critical value. Figures 7.18–7.21 show the stress-intensity factor as a function of flaw depth, aspect ratio, and loading condition.

The stress-intensity factors for the SEN flaws located on the top surface and the edge of the top flange of the cantilever beam were evaluated by the expression given for SEN specimens under pure bending given in Ref. 24 (p. 2.13). The expression is

$$K_I = \sigma \sqrt{\pi a} F\left(\frac{a}{b}\right),$$

where K_I is the stress-intensity factor, σ is the maximum bending stress, a is the flaw depth, and b is the specimen depth. Figures 7.22–7.25 show the stress-intensity factor as a function of flaw depth for the SEN flaws located on the top surface of the top flange. Figures 7.26–7.29 show the stress-intensity factor as a function of flaw depth for SEN flaws located on the edge of the top flange of the cantilever beam.

Two methods of solution (Liu²⁵ and Newman and Raju²⁶) were used to evaluate the stress-intensity factor for the quarter-circular corner crack subtending the edge and top surface of the top flange of the cantilever beam. Both solutions are premised on a uniform stress field, and the stress used in the analyses was the sum of the maximum bending stresses resulting from the vertical and horizontal loads. In both cases, the maximum value of K_I on the crack front was used for calculating the critical flaw size.

Figures 7.30–7.35 show the maximum stress-intensity factor for the corner crack of the cantilever beam evaluated by the Liu method; Figs. 7.36–7.41 show the maximum stress-intensity factor for the corner crack of the cantilever beam evaluated by the Newman-Raju method.

Two methods were used for estimating the stress-intensity factor for the assumed circumferential flaw of the girder-to-cantilever beam bolts. One method is given in Ref. 24, and the other is by Brown and Srawley.²⁷ The calculated K_I values are given in Figs. 7.42 and 7.43;

Fig. 7.18. Stress-intensity factor vs crack depth for cantilever beam with semielliptical surface flaw (case 3A).

Fig. 7.19. Stress-intensity factor vs flaw depths for cantilever beam with semielliptical surface flaw (case 3B).

Fig. 7.20. Stress-intensity factor vs crack depth for cantilever beam with semielliptical surface flaw (case 6A).

Fig. 7.21. Stress-intensity factor vs crack depth for cantilever beam with semielliptical surface flaw (case 6B).

Fig. 7.22. Stress-intensity factor vs crack depth for cantilever beam with SEN flaw on top surface (case 3A).

Fig. 7.23. Stress-intensity factor vs crack depth for cantilever beam with SEN flaw on top surface (case 3B).

Fig. 7.24. Stress-intensity factor vs crack depth for cantilever beam with SEN flaw on top surface (case 6A).

Fig. 7.25. Stress-intensity factor vs crack depth for cantilever beam with SEN flaw on top surface (case 6B).

Fig. 7.26. Stress-intensity factor vs crack depth for cantilever beam with SEN flaw on edge (case 3A).

Fig. 7.27. Stress-intensity factor vs crack depth for cantilever beam with SEN flaw on edge (case 3B).

Fig. 7.28. Stress-intensity factor vs crack depth for cantilever beam with SEN flaw on edge (case 5A).

Fig. 7.29. Stress-intensity factor vs crack depth for cantilever beam with SEN flaw on edge (case 5B).

Fig. 7.30. Stress-intensity factor vs crack depth for cantilever beam with corner crack (case 3A, Liu model).

Fig. 7.31. Stress-intensity factor vs crack depth for cantilever beam with corner crack (case 3B, Liu model).

Fig. 7.32. Stress-intensity factor vs crack depth for cantilever beam with corner crack (case 5A, Liu model).

Fig. 7.33. Stress-intensity factor vs crack depth for cantilever beam with corner crack (case 5B, Liu model).

Fig. 7.34. Stress-intensity factor vs crack depth for cantilever beam with corner crack (case 6A, Liu model).

Fig. 7.35. Stress-intensity factor vs crack depth for cantilever beam with corner crack (case 6B, Liu model).

Fig. 7.36. Stress-intensity factor vs crack depth for cantilever beam with corner crack (case 3A, Newman-Raju model).

Fig. 7.37. Stress-intensity factor vs crack depth for cantilever beam with corner crack (case 3B, Newman-Raju model).

Fig. 7.38. Stress-intensity factor vs crack depth for cantilever beam with corner crack (case 5A, Newman-Raju model).

Fig. 7.39. Stress-intensity factor vs crack depth for cantilever beam with corner crack (case 5B, Newman-Raju model).

Fig. 7.40. Stress-intensity factor vs crack depth for cantilever beam with corner crack (case 6A, Newman-Raju model).

Fig. 7.41. Stress-intensity factor vs crack depth for cantilever beam with corner crack (case 6B, Newman-Raju model).

Fig. 7.42. Stress-intensity factor vs crack depth for girder-to-cantilever-beam bolt circumferential flaw (case 3A).

Fig. 7.43. Stress-intensity factor vs crack depth for girder-to-cantilever-beam bolt circumferential flaw (case 5A).

as indicated, the agreement between the two methods is good. A similar calculation of stress-intensity factor for an assumed pretightening load of the bolts is shown in Fig. 7.44.

As previously noted, the method developed by Merkle was used to evaluate the stress-intensity factor for assumed semielliptical flaws in the lateral restraint; results are shown in Figs. 7.45 and 7.46.

Fig. 7.44. Stress-intensity factor vs crack depth for girder-to-cantilever-beam bolt with circumferential flaw for assumed tightening load.

7.9 Discussion of Results and Conclusions

Using the definition of the fracture-toughness temperature parameter ($T - \text{NDTT}$) developed in Sect. 7.4 as a Gaussian distribution with the values for ΔNDTT taken from Table 7.3, Table 7.6 provides a summary of $T - \text{NDTT}$ as a function of method of extrapolation of ΔNDTT , Gaussian

Fig. 7.45. Stress-intensity factor vs crack depth for lateral restraint with semielliptical surface flaw (case 3A).

Fig. 7.46. Stress-intensity factor vs crack depth for lateral restraint with semielliptical surface flaw (case 5A).

Table 7.6. Summary of (T - NDTT) as a function of Gaussian point and service life for each component

Component	T - NDTT (°F)									
	Method A Δ NDTT (Gaussian point)					Method B Δ NDTT (Gaussian point)				
	-3 σ	-1 σ	Mean	1 σ	3 σ	-3 σ	-1 σ	Mean	1 σ	3 σ
<i>0.0 EPFY</i>										
Cantilever beam Point 9, L = 13 in.	-30	-4.8	50	104.8	130	-30	-4.8	50	104.8	130
Cantilever beam Point 10, L = 17 in.	-22	3.2	58	112.8	138	-22	3.2	58	112.8	138
Girder-to-cantilever- beam bolt Point 4	-40	-14.8	40	94.8	120	-40	-14.8	40	94.8	120
Lateral restraint Point 8	230	245.8	280	314.2	330	230	245.8	280	314.2	330
<i>7.04 EPFY</i>										
Cantilever beam Point 9, L = 13 in.	-30	-4.8	50	104.8	130	-60	-34.8	20	74.8	100
Cantilever beam Point 10, L = 17 in.	-22	3.2	58	112.8	138	-42	16.8	38	92.8	118
Girder-to-cantilever- beam bolt Point 4	-40	-14.8	40	94.8	120	-85	-59.8	-5	49.8	75
Lateral restraint Point 8	230	245.8	280	314.2	330	230	241	265	289	300
<i>11.79 EPFY</i>										
Cantilever beam Point 9, L = 13 in.	-30	-4.8	50	104.8	130	-70	-44.8	10	64.8	90
Cantilever beam Point 10, L = 17 in.	-22	3.2	58	112.8	138	-52	26.8	28	82.8	108
Girder-to-cantilever- beam bolt Point 4	-80	-54.8	0	54.8	80	-105	-79.8	-25	29.8	55
Lateral restraint Point 8	200	215.8	250	284.2	300	190	205.8	240	274.2	290
<i>32 EPFY</i>										
Cantilever beam Point 9, L = 13 in.	-160	-134.8	-80	-25.2	0	-105	-79.8	-25	29.8	55
Cantilever beam Point 10, L = 17 in.	-52	-26.8	28	82.8	108	-77	-51.8	3	57.8	83
Girder-to-cantilever- beam bolt Point 4	-110	-84.8	-30	24.8	50	-115	-89.8	-35	19.8	45
Lateral restraint Point 8	100	115.8	150	184.2	200	150	165.8	200	234.2	250

point, and service life at 0, 7.04, 11.79, and 32 EFPY for each of the locations being evaluated for each of the structural components. Table 7.7 was developed from Table 7.6 by reading the value of toughness, K_{Ic} or K_{IR} , from Fig. 7.13 for each value of $T - NDTT$.

Equating K_{Ic} or K_{IR} , given in Table 7.7, to K_I in Figs. 7.18–7.46 permits an estimate of the critical flaw size for the various parameters considered in Table 7.7. Following this procedure, Table 7.8 provides a summary of the estimated critical flaw sizes for the cantilever beam for semicircular, 6:1-semielliptical surface, SEN top-surface, SEN edge, and corner quarter-circular flaws, both methods of extrapolation of $\Delta NDTT$, evaluation per ASME K_{Ic} and K_{IR} and for each loading condition of interest, as a function of the Gaussian point stress-intensity factor temperature parameter and as a function of service life.

The significant points to be noted about Table 7.8 are the following:

1. The calculated mean value of flaw size represents the best estimate.
2. In every loading case involving both horizontal and vertical loading, the critical corner crack is the smallest of the various types and consequently the type of greatest concern.
3. The use of Method A vs Method B for extrapolation of $\Delta NDTT$ data does not lead to significantly different results for this evaluation.
4. Very little difference in critical flaw size exists between evaluations at the $L = 17$ -in. vs $L = 13$ -in. locations.

Table 7.9, a summary of the estimated critical flaw sizes for girder-to-cantilever-beam bolts, was developed in the same manner as Table 7.8. Variables considered in Table 7.8 include both methods of extrapolation of $\Delta NDTT$, evaluation by ASME K_{Ic} and K_{IR} curves, two loading cases, the assumed temperature-parameter Gaussian distribution, and service life. In general, the points made about Table 7.8 apply also to Table 7.9. The small best-estimate critical flaw sizes for loading Case 5 and K_{IR} evaluation are small enough to be of concern. However, tightening of the bolting at the time of erection tends to alleviate the problem. Table 7.10 provides a summary of the best-estimate critical flaw size as a function of $NDTT_o$, operating temperature T , and evaluation by both ASME K_{Ic} and K_{IR} curves for pretightening to 70% of the minimum yield load, that is, 160 kips. It is evident that the small critical flaw sizes of Table 7.9 would be eliminated by the application of a pretightening load as noted. The Turkey Point supports were specified to be installed according to Ref. 28 in accordance with the standards of the American Institute for Steel Construction (AISC). AISC practice requires that bolting conforming to ASTM A325 or A490 be preloaded to 70% of the minimum tensile strength.²⁸

An examination of Table 7.7 shows that the estimated fracture toughness for the lateral restraints for both methods of $\Delta NDTT$ shift, for both K_{Ic} and K_{IR} evaluations, and as a function of service life is consistently high. The best-estimate value at 32 EFPY is >200 ksi/in., and the smallest value (-3σ , Method A $\Delta NDTT$) is 80 ksi/in., which corresponds to a critical flaw size of ~ 1.2 in. (Fig. 7.46). Thus, the lateral restraints do not appear to be the critical component in the support assembly.

Table 7.7. Summary of estimated fracture toughness from ASME K_{Ic} and K_{IR} curves as a function of Gaussian point and service life for each component

Component	Fracture toughness, K_{Ic} (K_{IR}) (ksi√in.)									
	Method A ΔNDTT (Gaussian point)					Method B ΔNDTT (Gaussian point)				
	-3σ	-1σ	Mean	1σ	3σ	-3σ	-1σ	Mean	1σ	3σ
<i>0.0 EPFY</i>										
Cantilever beam Point 9, L = 13 in.	44.5 (35)	52.5 (39)	89.5 (52.5)	>200 (84.5)	>200 (110)	44.5 (35)	52.5 (39)	89.5 (52.5)	>200 (84.5)	>200 (110)
Cantilever beam Point 10, L = 17 in.	47 (36)	53 (39)	99.5 (56)	>200 (91.5)	>200 (120)	47 (36)	53 (39)	99.5 (56)	>200 (91.5)	>200 (120)
Girder-to-cantilever- beam bolt Point 4	42.5 (34)	49 (37)	126 (64)	171 (77)	>200 (98.5)	42.5 (34)	49 (37)	126 (64)	171 (77)	>200 (98.5)
Lateral restraint Point 8	>200 (>200)	>200 (>200)	>200 (>200)	>200 (>200)	>200 (>200)	>200 (>200)	>200 (>200)	>200 (>200)	>200 (>200)	>200 (>200)
<i>7.04 EPFY</i>										
Cantilever beam Point 9, L = 13 in.						39 (32)	43.5 (34)	64 (44)	126 (64)	186 (80)
Cantilever beam Point 10, L = 17 in.						42 (34)	48 (36.5)	78 (49)	166 (75)	>200 (97)
Girder-to-cantilever- beam bolt Point 4						37 (30.5)	39 (32)	52 (38.5)	89.5 (53)	126 (64)
Lateral restraint Point 8						>200 (>200)	>200 (>200)	>200 (>200)	>200 (>200)	>200 (>200)
<i>11.79 EPFY</i>										
Cantilever beam Point 9, L = 13 in.						38 (31.5)	41.5 (33)	58.5 (41.5)	109.5 (59)	157.5 (78)
Cantilever beam Point 10, L = 17 in.						40 (33)	45 (35)	70 (46)	143 (69)	>200 (87)
Girder-to-cantilever- beam bolt Point 4						36 (30)	37 (31)	46 (36)	71 (46)	96 (54)
Lateral restraint Point 8						>200 (>200)	>200 (>200)	>200 (>200)	>200 (>200)	>200 (>200)
<i>32 EPFY</i>										
Cantilever beam Point 9, L = 13 in.	36 (30)	36 (30)	37 (31)	46 (36)	54 (39)	36 (30)	37 (31)	46 (36)	71 (46)	95.5 (54)
Cantilever beam Point 10, L = 17 in.	40 (33)	45 (35)	70 (46)	142 (69)	>200 (87)	37 (31)	40 (33)	55 (40)	100 (56)	143 (69)
Girder-to-cantilever- beam bolt Point 4	36 (30)	37 (30.5)	45 (35)	67 (45)	89.5 (53)	36 (30)	37 (30)	44 (34)	64 (44)	84 (51)
Lateral restraint Point 8	186 (80)	>200 (94)	>200 (137)	>200 (>200)	>200 (>200)	>200 (137)	>200 (164)	>200 (>200)	>200 (>200)	>200 (>200)

Table 7.8. Summary of estimated critical flaw sizes for cantilever beam as a function of type of flaw, ANDTT method, Gaussian point, ASME K_{Ic} or K_{IR} evaluation, location, loading case, and service life

			Critical flaw depth (in.)																			
Case No.	Loading condition	Gaussian point	Method A ANDTT										Method B ANDTT									
			Semicircular		6:1 Semi- elliptical		SEN top surface		SEN edge		Corner		Semicircular		6:1 Semi- elliptical		SEN top surface		SEN edge		Corner	
			K _{IC}	K _{IR}	K _{IC}	K _{IR}	K _{IC}	K _{IR}	K _{IC}	K _{IR}	K _{IC}	K _{IR}	K _{IC}	K _{IR}	K _{IC}	K _{IR}	K _{IC}	K _{IR}	K _{IC}	K _{IR}	K _{IC}	K _{IR}
			0.0 EPFY																			
3A	DL + E ⁺ (DL + SSE) L = 17 in.	-3σ	2.1	2.0	2.0	1.9	>2.4	>2.4	>2.4	1.5	1.5	0.9	2.1	2.0	2.0	1.9	>2.4	>2.4	>2.4	1.5	1.5	0.9
		-1σ	2.2	2.0	2.1	1.9	>2.4	>2.4	>2.4	1.8	1.9	1.0	2.2	2.0	2.1	1.9	>2.4	>2.4	>2.4	1.8	1.9	1.0
		Mean	>2.4	2.2	>2.4	2.1	>2.4	>2.4	>2.4	>2.4	>2.4	2.1	>2.4	2.2	>2.4	2.1	>2.4	>2.4	>2.4	>2.4	>2.4	2.1
		1σ	>2.4	>2.4	>2.4	>2.4	>2.4	>2.4	>2.4	>2.4	>2.4	>2.4	>2.4	>2.4	>2.4	>2.4	>2.4	>2.4	>2.4	>2.4	>2.4	>2.4
		3σ	>2.4	>2.4	>2.4	>2.4	>2.4	>2.4	>2.4	>2.4	>2.4	>2.4	>2.4	>2.4	>2.4	>2.4	>2.4	>2.4	>2.4	>2.4	>2.4	>2.4
3B	DL + E ⁺ (DL + SSE) L = 13 in.	-3σ	2.2	2.1	2.2	2.0	>2.4	>2.4	>2.4	>2.4	2.2	1.4	2.2	2.1	2.2	2.0	>2.4	>2.4	>2.4	>2.4	2.2	1.4
		-1σ	2.3	2.2	2.2	2.1	>2.4	>2.4	>2.4	>2.4	>2.4	1.7	2.3	2.2	2.2	2.1	>2.4	>2.4	>2.4	>2.4	>2.4	1.7
		Mean	>2.4	2.3	>2.4	2.2	>2.4	>2.4	>2.4	>2.4	>2.4	>2.4	>2.4	2.3	>2.4	2.2	>2.4	>2.4	>2.4	>2.4	>2.4	>2.4
		1σ	>2.4	>2.4	>2.4	>2.4	>2.4	>2.4	>2.4	>2.4	>2.4	>2.4	>2.4	>2.4	>2.4	>2.4	>2.4	>2.4	>2.4	>2.4	>2.4	>2.4
		3σ	>2.4	>2.4	>2.4	>2.4	>2.4	>2.4	>2.4	>2.4	>2.4	>2.4	>2.4	>2.4	>2.4	>2.4	>2.4	>2.4	>2.4	>2.4	>2.4	>2.4
5A	DL + LOCA(H) L = 17 in.	-3σ							1.2	0.7	0.4	0.2							1.2	0.7	0.4	0.2
		-1σ							1.5	0.8	0.5	0.3							1.5	0.8	0.5	0.3
		Mean							>2.4	1.7	>1.6	0.6							>2.4	1.7	>1.6	0.6
		1σ							>2.4	>2.4	>1.6	1.6							>2.4	>2.4	>1.6	1.6
		3σ							>2.4	>2.4	>1.6	>1.6							>2.4	>2.4	>1.6	>1.6
5B	DL + LOCA(H) L = 13 in.	-3σ							1.9	1.1	0.7	0.4							1.9	1.1	0.7	0.4
		-1σ							>2.4	1.4	0.9	0.5							>2.4	1.4	0.9	0.5
		Mean							>2.4	>2.4	>2.4	0.9							>2.4	>2.4	>2.4	0.9
		1σ							>2.4	>2.4	>2.4	1.9							>2.4	>2.4	>2.4	1.9
		3σ							>2.4	>2.4	>2.4	>2.4							>2.4	>2.4	>2.4	>2.4
6A	DL + LOCA(45°) L = 17 in.	-3σ	1.2	0.9	1.0	0.7	1.0	0.5			0.5	0.3	1.2	0.9	1.0	0.7	1.0	0.5			0.5	0.3
		-1σ	1.4	1.0	1.1	0.8	1.2	0.6			0.6	0.3	1.4	1.0	1.1	0.8	1.2	0.6			0.6	0.3
		Mean	>1.9	1.4	>1.7	1.2	>2.4	1.4			>1.9	0.7	>1.9	1.4	>1.7	1.2	>2.4	1.4			>1.9	0.7
		1σ	>1.9	1.8	>1.7	>1.7	>2.4	>2.4			>1.9	1.8	>1.9	1.8	>1.7	>1.7	>2.4	>2.4			>1.9	1.8
		3σ	>1.9	>1.9	>1.7	>1.7	>2.4	>2.4			>1.9	>1.9	>1.9	>1.9	>1.7	>1.7	>2.4	>2.4			>1.9	>1.9
6B	DL + LOCA(45°) L = 17 in.	-3σ	1.5	1.2	1.2	1.0	1.5	0.9			0.7	0.4	1.5	1.2	1.2	1.0	1.5	0.9			0.7	0.5
		-1σ	1.6	1.3	1.4	1.1	2.1	1.1			1.0	0.6	1.6	1.3	1.4	1.1	2.1	1.1			1.0	0.6
		Mean	2.0	1.6	1.9	1.4	>2.4	2.1			>2.5	1.0	2.0	1.6	1.9	1.4	>2.4	2.1			>2.5	1.0
		1σ	>2.0	2.0	>1.9	1.9	>2.4	>2.4			>2.5	2.5	>2.0	2.0	>1.9	1.9	>2.4	>2.4			>2.5	>2.5
		3σ	>2.0	>2.0	>1.9	>1.9	>2.4	>2.4			>2.5	>2.5	>2.0	>2.0	>1.9	>1.0	>2.4	>2.4			>2.5	>2.5

Table 7.8 (continued)

			Critical flaw depth (in.)																			
Case No.	Loading condition	Gaussian point	Method A Δ NDTT										Method B Δ NDTT									
			Semicircular		6:1 Semi- elliptical		SEN top surface		SEN edge		Corner		Semicircular		6:1 Semi- elliptical		SEN top surface		SEN edge		Corner	
			K_{Ic}	K_{IR}	K_{Ic}	K_{IR}	K_{Ic}	K_{IR}	K_{Ic}	K_{IR}	K_{Ic}	K_{IR}	K_{Ic}	K_{IR}	K_{Ic}	K_{IR}	K_{Ic}	K_{IR}	K_{Ic}	K_{IR}	K_{Ic}	K_{IR}
7.04 EPFY																						
3A	DL + E' (DL + SSE) L = 17 in.	-3 σ										2.1	1.9	2.0	1.8	>2.4	>2.4	2.1	1.4	1.2	0.8	
		-1 σ										2.1	2.0	2.0	1.9)	>2.4	>2.4	>2.4	1.6	1.5	0.9	
		Mean										2.4	2.2	2.3	2.0	>2.4	>2.4	>2.4	>2.4	>2.4	1.7	
		1 σ										>2.4	2.3	>2.4	2.2	>2.4	>2.4	>2.4	>2.4	>2.4	>2.4	
		3 σ										>2.4	>2.4	>2.4	>2.4	>2.4	>2.4	>2.4	>2.4	>2.4	>2.4	
3B	DL + E' (DL + SSE) L = 13 in.	-3 σ										2.2	2.1	2.2	2.0	>2.4	>2.4	>2.4	2.1	1.7	1.2	
		-1 σ										2.2	2.1	2.2	2.0	>2.4	>2.4	>2.4	2.4	2.3	1.3	
		Mean										2.3	2.3	2.3	2.2	>2.4	>2.4	>2.4	>2.4	>2.4	2.2	
		1 σ										>2.4	>2.4	>2.4	2.3	>2.4	>2.4	>2.4	>2.4	>2.4	>2.4	
		3 σ										>2.4	>2.4	>2.4	2.3	>2.4	>2.4	>2.4	>2.4	>2.4	>2.4	
5A	DL + LOCA(H) L = 17 in.	-3 σ																0.9	0.6	0.3	0.2	
		-1 σ																1.2	0.7	0.4	0.3	
		Mean																>2.4	1.3	1.2	0.5	
		1 σ																>2.4	2.3	>1.6	1.1	
		3 σ																>2.4	>2.4	>1.6	>1.6	
5B	DL + LOCA(H) L = 13 in.	-3 σ																1.4	0.9	0.5	0.3	
		-1 σ																1.8	1.1	0.6	0.4	
		Mean																>2.4	1.9	1.4	0.6	
		1 σ																>2.4	>2.4	>2.4	1.3	
		3 σ																>2.4	>2.4	>2.4	2.0	
6A	DL + LOCA(45°) L = 17 in.	-3 σ										1.1	0.9	0.9	0.6	0.8	0.4			0.4	0.2	
		-1 σ										1.3	0.9	1.0	0.7	1.0	0.6			0.5	0.3	
		Mean										1.7	1.3	1.6	1.0	>2.4	1.0			1.3	0.5	
		1 σ										>1.9	1.7	>1.7	>1.7	>2.4	>2.4			>1.9	1.3	
		3 σ										>1.9	1.9	>1.7	>1.7	>2.4	>2.4			>1.9	>1.9	
6B	DL + LOCA(45°) L = 13 in.	-3 σ										1.3	1.1	1.1	0.8	1.1	0.7			0.6	0.4	
		-1 σ										1.4	1.2	1.2	0.9	1.4	0.8			0.7	0.4	
		Mean										1.8	1.4	1.6	1.2	>2.4	1.4			1.6	0.7	
		1 σ										>1.9	1.8	>1.9	1.6	>2.4	>2.4			>2.5	1.6	
		3 σ										>1.9	1.9	>1.9	1.8	>2.4	>2.4			>2.5	>2.5	

Table 7.8 (continued)

Case No.	Loading condition	Gaussian point	Critical flaw depth (in.)																			
			Method A Δ NDTT										Method B Δ NDTT									
			Semicircular		6:1 Semi-elliptical		SEN top surface		SEN edge		Corner		Semicircular		6:1 Semi-elliptical		SEN top surface		SEN edge		Corner	
			K_{Ic}	K_{IR}	K_{Ic}	K_{IR}	K_{Ic}	K_{IR}	K_{Ic}	K_{IR}	K_{Ic}	K_{IR}	K_{Ic}	K_{IR}	K_{Ic}	K_{IR}	K_{Ic}	K_{IR}	K_{Ic}	K_{IR}	K_{Ic}	K_{IR}
			11.79 EPPY																			
3A	DL + E" (DL + SSE) L = 17 in.	-3 σ																				
		-1 σ																				
		Mean																				
		1 σ																				
		3 σ																				
3B	DL + E" (DL + SSE) L = 13 in.	-3 σ																				
		-1 σ																				
		Mean																				
		1 σ																				
		3 σ																				
5A	DL + LOCA(H) L = 17 in.	-3 σ																				
		-1 σ																				
		Mean																				
		1 σ																				
		3 σ																				
5B	DL + LOCA(H) L = 13 in.	-3 σ																				
		-1 σ																				
		Mean																				
		1 σ																				
		3 σ																				
6A	DL + LOCA(45°) L = 17 in.	-3 σ																				
		-1 σ																				
		Mean																				
		1 σ																				
		3 σ																				
6B	DL + LOCA(45°H) L = 13 in.	-3 σ																				
		-1 σ																				
		Mean																				
		1 σ																				
		3 σ																				

Table 7.8 (continued)

			Critical flaw depth (in.)																			
Case No.	Loading condition	Gaussian point	Method A Δ NDTT										Method B Δ NDTT									
			Semicircular		6:1 Semi- elliptical		SEN top surface		SEN edge		Corner		Semicircular		6:1 Semi- elliptical		SEN top surface		SEN edge		Corner	
			K _{Ic}	K _{IR}	K _{Ic}	K _{IR}	K _{Ic}	K _{IR}	K _{Ic}	K _{IR}	K _{Ic}	K _{IR}	K _{Ic}	K _{IR}	K _{Ic}	K _{IR}	K _{Ic}	K _{IR}	K _{Ic}	K _{IR}	K _{Ic}	K _{IR}
32 EPPI																						
3A	DL + E ^r (DL + SSE) L = 17 in.	-3 σ	2.0	1.9	1.9	1.8	>2.5	>2.5	2.0	1.3	1.1	0.7	2.0	1.9	1.9	1.7	>2.5	>2.5	1.7	1.1	0.9	0.6
		-1 σ	2.1	2.0	2.0	1.8	>2.5	>2.5	>2.5	1.5	1.4	0.8	2.0	1.9	1.9	1.8	>2.5	>2.5	2.0	1.3	1.1	0.7
		Mean	2.3	2.1	2.2	2.0	>2.5	>2.5	>2.5	>2.5	>2.5	1.4	2.2	2.0	2.1	1.9	>2.5	>2.5	>2.5	1.9	2.0	1.1
		1 σ	>2.4	2.3	>2.3	2.2	>2.5	>2.5	>2.5	>2.5	>2.5	>2.5	>2.4	2.2	>2.3	2.1	>2.5	>2.5	>2.5	>2.5	>2.5	>2.5
		3 σ	>2.4	2.4	>2.3	2.3	>2.5	>2.5	>2.5	>2.5	>2.5	>2.5	>2.4	2.3	>2.3	2.2	>2.5	>2.5	>2.5	>2.5	>2.5	>2.5
3B	DL + E ^r (DL + SSE) L = 13 in.	-3 σ	2.1	2.0	2.1	2.0	>2.5	>2.5	>2.5	1.9	1.5	1.0	2.1	2.0	2.1	2.0	>2.5	>2.5	>2.5	1.9	1.5	1.0
		-1 σ	2.1	2.0	2.1	2.0	>2.5	>2.5	>2.5	1.9	1.5	1.0	2.2	2.1	2.2	2.0	>2.5	>2.5	>2.5	2.0	1.6	1.1
		Mean	2.2	2.1	2.1	2.0	>2.5	>2.5	>2.5	2.0	1.6	1.1	2.3	2.1	2.2	2.0	>2.5	>2.5	>2.5	>2.5	2.4	1.5
		1 σ	2.2	2.1	2.1	2.1	>2.5	>2.5	>2.5	>2.5	2.3	1.5	2.3	2.2	2.3	2.2	>2.5	>2.5	>2.5	>2.5	>2.5	2.4
		3 σ	2.3	2.2	2.2	2.1	>2.5	>2.5	>2.5	>2.5	>2.5	1.7	2.4	2.3	2.4	2.2	>2.5	>2.5	>2.5	>2.5	>2.5	>2.5
5A	DL + LOCA(H) L = 17 in.	-3 σ							0.8	0.6	0.3	0.2							0.7	0.5	0.3	0.2
		-1 σ							1.1	0.6	0.4	0.2							0.8	0.6	0.3	0.2
		Mean							>2.5	1.1	1.0	0.4							1.7	0.8	0.6	0.3
		1 σ							>2.5	>2.5	>1.6	0.9							>2.5	1.7	>1.5	0.6
		3 σ							>2.5	>2.5	>1.6	1.5							>2.5	>2.5	>1.5	0.9
5B	DL + LOCA(H) L = 13 in.	-3 σ							1.2	0.8	0.4	0.3							1.2	0.8	0.4	0.3
		-1 σ							1.2	0.8	0.4	0.3							1.3	0.9	0.5	0.3
		Mean							1.3	0.8	0.5	0.3							2.0	1.2	0.7	0.4
		1 σ							2.0	1.2	0.7	0.4							>2.5	2.0	1.6	0.7
		3 σ							>2.5	1.5	1.0	0.5							>2.5	>2.5	>2.5	1.0
6A	DL + LOCA(45°) L = 17 in.	-3 σ	1.1	0.8	0.8	0.6	0.7	0.4			0.4	0.2	1.0	0.8	0.7	0.5	0.6	0.4			0.3	0.2
		-1 σ	1.2	0.9	1.0	0.7	0.9	0.5			0.4	0.3	1.1	0.8	0.8	0.6	0.7	0.4			0.3	0.2
		Mean	1.6	1.2	1.5	1.0	2.2	0.9			1.1	0.4	1.4	1.1	1.2	0.8	1.3	0.7			0.7	0.35
		1 σ	>1.8	1.6	>1.8	1.4	>2.5	2.2			>1.9	1.1	1.9	1.4	1.8	1.2	>2.5	1.4			>1.9	0.7
		3 σ	>1.8	1.8	>1.8	1.7	>2.5	>2.5			>1.9	1.7	>1.9	1.6	>1.8	1.4	>2.5	2.2			>1.9	1.1
6B	DL + LOCA(45°) L = 13 in.	-3 σ	1.2	1.0	1.0	0.8	1.0	0.7			0.5	0.3	1.2	1.0	1.0	0.8	1.0	0.7			0.5	0.3
		-1 σ	1.2	1.0	1.0	0.8	1.0	0.7			0.5	0.3	1.3	1.1	1.0	0.8	1.0	0.7			0.5	0.4
		Mean	1.3	1.1	1.0	0.8	1.0	0.7			0.5	0.3	1.4	1.2	1.3	1.0	1.6	1.0			0.8	0.5
		1 σ	1.4	1.2	1.3	1.0	1.6	1.0			0.8	0.5	1.8	1.5	1.7	1.3	>2.5	1.6			>2.5	0.8
		3 σ	1.6	1.3	1.5	1.1	2.3	1.2			1.1	0.6	2.1	1.6	1.9	1.5	>2.5	2.3			>2.5	1.1

Table 7.9. Summary of critical flaw sizes for girder-to-cantilever beam bolts as a function of Δ NDTT method, Gaussian point, loading condition, and service life

Case No.	Loading condition	Temperature parameter Gaussian point	Critical flaw depth (in.)															
			Method A Δ NDTT								Method B Δ NDTT							
			Service life, EFPY								Service life, EFPY							
			0.0		7.04		11.79		32		0.0		7.04		11.79		32	
			K_{Ic}	K_{IR}	K_{Ic}	K_{IR}	K_{Ic}	K_{IR}	K_{Ic}	K_{IR}	K_{Ic}	K_{IR}	K_{Ic}	K_{IR}	K_{Ic}	K_{IR}	K_{Ic}	K_{IR}
3	DL + E ^c (DL + SSE)	-3 σ	0.58	0.53					0.54	0.5	0.58	0.53	0.55	0.5	0.54	0.5	0.54	0.5
		-1 σ	0.6	0.55					0.55	0.51	0.6	0.55	0.56	0.52	0.53	0.51	0.55	0.5
		Mean	>0.7	0.64					0.57	0.54	>0.7	0.64	0.61	0.55	0.58	0.54	0.58	0.53
		1 σ	>0.7	0.67					0.65	0.58	>0.7	0.67	0.68	0.61	0.65	0.59	0.64	0.58
		3 σ	>0.7	0.7					0.68	0.61	>0.7	0.7	>0.7	0.64	0.69	0.62	0.68	0.61
5	DL + LOCA(H)	-3 σ	0.2	0.14					0.16	0.11	0.2	0.14	0.17	0.12	0.16	0.11	0.16	0.11
		-1 σ	0.24	0.17					0.17	0.12	0.24	0.17	0.18	0.15	0.17	0.12	0.27	0.11
		Mean	>0.4	0.32					0.22	0.15	>0.4	0.32	0.26	0.18	0.23	0.16	0.22	0.15
		1 σ	>0.4	0.37					0.33	0.22	>0.4	0.37	0.41	0.27	0.35	0.23	0.32	0.22
		3 σ	>0.4	0.43					0.41	0.27	>0.4	0.43	>0.4	0.32	0.42	0.27	0.39	0.25

Table 7.10. Summary of girder-to-cantilever-beam bolt critical flaw sizes with pretightening to 70% yield load (160 kips) as a function of $NDTT_o$, operating temperature, and assumed fracture toughness (ASME K_{Ic} or K_{IR})

T (°F)	$NDTT_o$ (°F)	T - $NDTT_o$ (°F)	ASME K_{Ic} (ksi/ $\sqrt{\text{in.}}$)	A (K_{Ic}) (in.)	ASME K_{IR} (ksi/ $\sqrt{\text{in.}}$)	A (K_{IR}) (in.)
130	-40	170	>200	>0.3	175	>0.3
	-20	150	>200	>0.3	137	>0.3
	0	130	>200	>0.3	110	0.3
	10	120	>200	>0.3	99	0.27
	40	90	158	>0.3	73	0.18
	70	60	102	0.28	61	0.13
	100	30	71	0.18	46	0.06
100	-40	140	>200	>0.3	122	>0.3
	-20	120	>200	>0.3	98	0.27
	0	100	187	>0.3	80	0.21
	10	90	158	>0.3	73	0.18
	40	60	102	0.28	56	0.11
	70	30	71	0.18	46	0.06
	100	0	54	0.1	39	0.03

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8. CONCLUSIONS AND RECOMMENDATIONS

8.1 Conclusions

1. Best-estimate minimum critical flaw sizes corresponding to the most severe credible loading condition at 32 EFPY are small enough to be of concern for both Trojan and Turkey Point.

2. Considering the type of structural components, flaws, and flaw locations evaluated for both the Trojan and Turkey Point vessel support structures, crack arrest is not likely if flaw propagation takes place.

3. A critical location for the flaws, referred to in item 1, is in a portion of the support structure embedded in the concrete biological shield, making inspection for flaws in one critical area very difficult, if not impossible.

4. No specific safety factors were applied in the evaluation, and thus their consideration would reduce the size of the allowable flaws.

5. The use of the ASME K_{IR} fracture-toughness curve for the Turkey Point best-estimate analysis does in a sense represent the application of a safety factor. This conservative approach was used because of a lack of system dynamic response data and specific-material fracture-toughness data.

6. Modification of the K_{IR} curve for lack of plane-strain conditions at 11.8 EFPY was not considered for Turkey Point, but presumably would result in larger critical flaw sizes if it were. (It was considered in the Trojan analysis.)

7. Significant uncertainties in the calculation of critical flaw size result in a broad range of critical sizes. More definitive data on radiation-induced embrittlement, fracture toughness, and operating temperature could result in substantially larger critical flaw sizes. Of course, the opposite is true as well.

8. The minimum critical flaw size for Trojan is associated with a somewhat unique design feature (grout hole in a critical structural member). Critical flaw sizes at other locations are considerably greater but not necessarily inconsequential.

9. Consistent with information supplied by the utility, the LOCA was used as the most severe loading condition for Turkey Point. A utility/NRC reevaluation of the need to do this, based perhaps on leak-before-break considerations, could result in the definition of a less severe loading condition for critical-flaw-size evaluation. This could increase the calculated minimum critical flaw size substantially.

10. Low-cycle fatigue is not a mechanism for creating flaws of critical size.

11. Best-estimate minimum critical flaw depths calculated for operating times consistent with the present date (1988) are double the 32-EFPY value for Trojan, and the type and location of the two flaws are different. For Turkey Point, the type, location, and size of the minimum critical flaw are the same for both times.

12. For Turkey Point, significant unequal horizontal forces associated with variations in sliding friction in the components that accommodate radial thermal expansion may exist and can decrease the critical flaw size appreciably.

8.2 Recommendations

1. An effort should be made to reduce the uncertainties in the material and operating data used as input to the evaluation. Parameters of particular concern are

- (a) interpretation and extrapolation of the HFIR surveillance data;
- (b) fracture-toughness of specific materials, including $NDTT_0$;
- (c) credible static and dynamic loading conditions (Turkey Point); and
- (d) operating temperature.

2. The likelihood of the existence of flaws of estimated critical size and larger should be evaluated. An examination of inspection procedures and results, if and when they become available, may be useful in this regard.

3. Because shield-tank and long-column supports are exposed to the maximum fluxes in the cavity, a review of utility/industry evaluations of these structures or an independent evaluation should be conducted.

4. The variation in horizontal loads resulting from unequal friction in the components that accommodate thermal radial expansion should be quantified.

APPENDIXES

Appendix 1

TROJAN SUPPORT BEAM ANALYSIS INPUT AND OUTPUT FILES

TROJAN NUCLEAR PLANT : REACTOR VESSEL SUPPORT

Table A. Elastic foundation model geometry

Beam Node Number	"Y" Ordinate (in.)	Length of Foundation (in.)	Foundation Width (in./in.)	Foundation Area (in. ²)	Foundation Modulus E (lb/in. ²)
1	0				
2	11.35	3.31	32.75	102.4025	6000000
3	13.63	1.25	16	20	29000000
4	16.5	4.5	44.25	197.125	6000000
5	19	0			
6	21	4.5	44.25	199.125	6000000
7	23	0			
8	25.5	4.5	44.25	199.125	6000000
9	28.37	1.25	16	20	29000000
10	31.54	5.09	32.75	166.6975	6000000
11	36.63	5.09	32.75	166.6975	6000000
12	41.72	5.09	32.75	166.6975	6000000
13	46.81	5.09	32.75	166.6975	6000000
14	51.9	5.09	32.75	166.6975	6000000
15	56.99	5.09	32.75	166.6975	6000000
16	62.08	5.09	32.75	166.6975	6000000
17	65.16	1.062	15.5	16.461	29000000
18	67.79	4.21	35.39	148.9919	6000000
19	72	4.21	35.39	148.9919	6000000
20	76.21	4.21	35.39	148.9919	6000000
21	78.844	1.062	15.5	16.461	29000000

Table B. Microsafe 2-D input file

39 /	NR		14	51.9	0 /
2 /	NR		15	56.99	0 /
38 /	NR		16	62.08	0 /
0 /	NR		17	65.16	0 /
0 /	NR		18	67.79	0 /
1 /	NR		19	72	0 /
35 /	NR		20	76.21	0 /
1	0	0 /	21	78.84	0 /
2	11.35	0 /	22	11.35	-29 /
3	13.63	0 /	23	13.63	-29 /
4	16.5	0 /	24	16.5	-29 /
5	19	0 /	25	21	-29 /
6	21	0 /	26	25.5	-29 /
7	23	0 /	27	28.37	-29 /
8	25.5	0 /	28	31.54	-29 /
9	28.37	0 /	29	36.63	-29 /
10	31.54	0 /	30	41.72	-29 /
11	36.63	0 /	31	46.81	-29 /
12	41.72	0 /	32	51.9	-29 /
13	46.81	0 /	33	56.99	-29 /

Table B (continued)

31	62.08	-29 /	23	1	0 /
35	65.16	-29 /	23	2	0 /
36	67.79	-29 /	23	3	0 /
37	72	-29 /	24	1	0 /
38	76.21	-29 /	24	2	0 /
39	78.84	-29 /	24	3	0 /
1	29000000	0.3 /	25	1	0 /
2	6000000	0.3 /	25	2	0 /
1	1	2 125.5 5487.4 1 0 0 /	25	3	0 /
2	2	3 125.5 5487.4 1 0 0 /	26	1	0 /
3	3	4 125.5 5487.4 1 0 0 /	26	2	0 /
4	4	5 125.5 5487.4 1 0 0 /	26	3	0 /
5	5	6 105.5 4275.8 1 0 0 /	27	1	0 /
6	6	7 105.5 4275.8 1 0 0 /	27	2	0 /
7	7	8 125.5 5487.4 1 0 0 /	27	3	0 /
8	8	9 125.5 5487.4 1 0 0 /	28	1	0 /
9	9	10 125.5 5487.4 1 0 0 /	28	2	0 /
10	10	11 125.5 5487.4 1 0 0 /	28	3	0 /
11	11	12 125.5 5487.4 1 0 0 /	29	1	0 /
12	12	13 125.5 5487.4 1 0 0 /	29	2	0 /
13	13	14 125.5 5487.4 1 0 0 /	29	3	0 /
14	14	15 125.5 5487.4 1 0 0 /	30	1	0 /
15	15	16 125.5 5487.4 1 0 0 /	30	2	0 /
16	16	17 125.5 5487.4 1 0 0 /	30	3	0 /
17	17	18 125.5 5487.4 1 0 0 /	31	1	0 /
18	18	19 125.5 5487.4 1 0 0 /	31	2	0 /
19	19	20 125.5 5487.4 1 0 0 /	31	3	0 /
20	20	21 125.5 5487.4 1 0 0 /	32	1	0 /
21	2	22 0.001 0.01 2 0 0 /	32	2	0 /
22	3	23 30 0.01 1 0 0 /	32	3	0 /
23	4	24 199.12 0.01 2 0 0 /	33	1	0 /
24	6	25 199.12 0.01 2 0 0 /	33	2	0 /
25	8	26 199.12 0.01 2 0 0 /	33	3	0 /
26	9	27 20 0.01 1 0 0 /	34	1	0 /
27	10	28 166.7 0.01 2 0 0 /	34	2	0 /
28	11	29 166.7 0.01 2 0 0 /	34	3	0 /
29	12	30 166.7 0.01 2 0 0 /	35	1	0 /
30	13	31 166.7 0.01 2 0 0 /	35	2	0 /
31	14	32 166.7 0.01 2 0 0 /	35	3	0 /
32	15	33 166.7 0.01 2 0 0 /	36	1	0 /
33	16	34 166.7 0.01 2 0 0 /	36	2	0 /
34	17	35 16.46 0.01 1 0 0 /	36	3	0 /
35	18	36 148.99 0.01 2 0 0 /	37	1	0 /
36	19	37 148.99 0.01 2 0 0 /	37	2	0 /
37	20	38 148.99 0.01 2 0 0 /	37	3	0 /
38	21	39 16.46 0.01 1 0 0 /	38	1	0 /
1	0 -100000	0 /	38	2	0 /
21	1	0 /	38	3	0 /
22	1	0 /	39	1	0 /
22	2	0 /	39	2	0 /
22	3	0 /	39	3	0 /

Table C. Solution (a): inboard
concrete assumed load bearing

Node Displacements

Node	U	V	Omega
1	0	-0.00437	0.000226
2	0	-0.00196	0.000185
3	0	-0.00156	0.000168
4	0	-0.00111	0.000145
5	0	-0.00077	0.000124
6	0	-0.00054	0.000104
7	0	-0.00036	0.000085
8	0	-0.00017	0.000067
9	0	-0.00000	0.000005
10	0	0.000129	0.000034
11	0	0.000248	0.000014
12	0	0.000284	0.000001
13	0	0.000269	-0.000000
14	0	0.000226	-0.000001
15	0	0.000171	-0.000001
16	0	0.000114	-0.000001
17	0	0.000008	-0.000001
18	0	0.000052	-0.000001
19	0	0.000009	-0.000001
20	0	-0.000003	-0.000001
21	0	-0.000006	-0.000001
22	0	0	0
23	0	0	0
24	0	0	0
25	0	0	0
26	0	0	0
27	0	0	0
28	0	0	0
29	0	0	0
30	0	0	0
31	0	0	0
32	0	0	0
33	0	0	0
34	0	0	0
35	0	0	0
36	0	0	0
37	0	0	0
38	0	0	0
39	0	0	0

Beam Corner Forces

BEAM	I	J	FX1	FY1	MZ1	FX2	FY2	MZ2
1	1	2	0	-100000	0	0	100000	
2	2	3	0	-55921	1134999	0	55921	
3	3	4	0	-24680	1262491	0	24680	
4	4	5	0	21161	1333322	0	-21161	
5	5	6	0	21161	1280420	0	-21161	-1135000
6	6	7	-1	43762	1238098	1	-43762	-1262498
7	7	8	-1	43762	1150574	1	-43762	-1333323

Table C (continued)

8	8	9	-1	50798	1041168	-1	-50798	-1280420
9	9	10	-1	50842	895404	1	-50842	-1238099
10	10	11	-1	46388	734234	1	-46388	-1150574
11	11	12	-1	37851	498121	1	-37851	-1041168
12	12	13	-1	28069	305460	1	-28069	-895406
13	13	14	-1	18803	162590	1	-18803	-734235
14	14	15	-1	11012	66882	1	-11012	-498121
15	15	16	-1	5106	10833	1	-5106	-305460
16	16	17	-1	1176	-15158	1	-1176	-162590
17	17	18	-1	-142	-18781	1	142	-66882
18	18	19	-1	-1746	-18408	1	1746	-10833
19	19	20	-1	-2013	-11057	1	2013	15158
20	20	21	-1	-981	-2581	1	981	18781
21	2	22	0	-44079	2	0	44079	18408
22	3	23	0	-31241	7	0	31241	11057
23	4	24	0	-45840	1	0	45840	2581
24	6	25	0	-22602	1	0	22602	0
25	8	26	0	-7026	1	0	7026	1
26	9	27	0	-54	2	0	54	3
27	10	28	0	4454	0	0	-4454	1
28	11	29	0	8537	0	0	-8537	0
29	12	30	0	9782	0	0	-9782	0
30	13	31	0	9265	0	0	-9265	1
31	14	32	0	7792	0	0	-7792	0
32	15	33	0	5905	0	0	-5905	0
33	16	34	0	3930	0	0	-3930	0
34	17	35	0	1318	0	0	-1318	0
35	18	36	0	1604	0	0	-1604	0
36	19	37	0	267	0	0	-267	0
37	20	38	0	-1032	0	0	1032	0
38	21	39	0	-981	0	0	981	0

Beam Loads and Stresses

Beam	I	J	PAX	SAX	SH1	SH2	BH1	BH2
1	1	2	0	0	100000	100000	0	-1135000
2	2	3	0	0	55921	55921	-1134999	-1262498
3	3	4	0	0	24680	24680	-1262492	-1333323
4	4	5	0	0	-21161	-21161	-1333322	-1280420
5	5	6	0	0	-21161	-21161	-1280420	-1238099
6	6	7	1	0	-43762	-43762	-1238098	-1150574
7	7	8	1	0	-43762	-43762	-1150574	-1041168
8	8	9	1	0	-50788	-50788	-1041168	-895406
9	9	10	1	0	-50842	-50842	-895404	-734235
10	10	11	1	0	-46388	-46388	-734234	-498121
11	11	12	1	0	-37851	-37851	-498121	-305460
12	12	13	1	0	-28069	-28069	-305460	-162590
13	13	14	1	0	-18803	-18803	-162590	-66882
14	14	15	1	0	-11012	-11012	-66882	-10833
15	15	16	1	0	-5106	-5106	-10833	15158
16	16	17	1	0	-1176	-1176	15158	18781
17	17	18	1	0	142	142	18781	18408

Table C (continued)

18	18	19	1	0	1746	1746	18408	11057
19	19	20	1	0	2013	2013	11057	2581
20	20	21	1	0	981	981	2581	0
21	2	22	-44079	-407	0	0	-2	1
22	3	23	-31241	-1562	0	0	-7	3
23	4	24	-45840	-230	0	0	1	1
24	6	25	-22602	-114	0	0	-1	0
25	8	26	-7026	-35	0	0	-1	0
26	9	27	-54	-3	0	0	-2	1
27	10	28	4434	27	0	0	0	0
28	11	29	8537	51	0	0	0	0
29	12	30	9782	59	0	0	0	0
30	13	31	9265	56	0	0	0	0
31	14	32	7792	47	0	0	0	0
32	15	33	5905	35	0	0	0	0
33	16	34	3930	24	0	0	0	0
34	17	35	1318	80	0	0	0	0
35	18	36	1604	11	0	0	0	0
36	19	37	267	2	0	0	0	0
37	20	38	-1032	-7	0	0	0	0
38	21	39	-981	-60	0	0	0	0

Node Internal Forces and Reactions

Node	X	Y	FX	FY	MZ
1	0	0	0	0	0
2	11.35	0	0	0	0
3	13.63	0	0	0	0
4	16.5	0	0	0	0
5	19	0	0	0	0
6	21	0	0	0	0
7	23	0	0	0	0
8	25.5	0	0	0	0
9	28.37	0	0	0	0
10	31.54	0	0	0	0
11	36.63	0	0	0	0
12	41.72	0	0	0	0
13	46.81	0	0	0	0
14	51.9	0	0	0	0
15	56.99	0	0	0	0
16	62.08	0	0	0	0
17	65.16	0	0	0	0
18	67.79	0	0	0	0
19	72	0	0	0	0
20	76.21	0	0	0	0
21	78.94	0	1	0	0
22	11.35	-29	0	44079	1
23	13.63	-29	0	31241	3
24	16.5	-29	0	45840	1
25	21	-29	0	22602	0
26	25.5	-29	0	7026	0
27	28.37	-29	0	54	1

Table C (continued)

28	31.54	-29	0	-4454	0
29	36.63	-29	0	-8537	0
30	41.72	-29	0	-9782	0
31	46.81	-29	0	-9265	0
32	51.9	-29	0	-7792	0
33	56.99	-29	0	-5905	0
34	62.08	-29	0	-3930	0
35	65.16	-29	0	-1313	0
36	67.79	-29	0	-1604	0
37	72	-29	0	-267	0
38	76.21	-29	0	1022	0
39	78.24	-29	0	981	0

Table D. Solution (b): inner concrete
assumed non load bearing

Node Displacements

Node	U	V	Omega
1	0	-0.00354	0.000269
2	0	-0.00264	0.000228
3	0	-0.00214	0.00021
4	0	-0.00157	0.000184
5	0	-0.00114	0.000161
6	0	-0.00084	0.000136
7	0	-0.00059	0.000113
8	0	-0.00034	0.000092
9	0	-0.00011	0.000067
10	0	0.000076	0.000049
11	0	0.000255	0.000023
12	0	0.000324	0.000006
13	0	0.000323	-0.000000
14	0	0.000281	-0.000001
15	0	0.000219	-0.000001
16	0	0.000151	-0.000001
17	0	0.000109	-0.000001
18	0	0.000075	-0.000001
19	0	0.000021	-0.000001
20	0	-0.000003	-0.000001
21	0	-0.000006	-0.000001
22	0	0	0
23	0	0	0
24	0	0	0
25	0	0	0
26	0	0	0
27	0	0	0
28	0	0	0
29	0	0	0
30	0	0	0
31	0	0	0
32	0	0	0

Table D (continued)

33	0	0	0
34	0	0	0
35	0	0	0
36	0	0	0
37	0	0	0
38	0	0	0
39	0	0	0

Beam Corner Forces

BEAM	I	J	FX1	FY1	MZ1	FX2	FY2	MZ2
1	1	2	0	-100000	0	0	100000	-1135000
2	2	3	0	-99999	1134993	0	99999	-1362997
3	3	4	-1	-57173	1362988	1	57173	-1527075
4	4	5	-1	7675	1527073	1	-7675	-1507885
5	5	6	-1	7675	1507885	1	-7675	-1492535
6	6	7	-1	42537	1492534	1	-42537	-1407460
7	7	8	-1	42537	1407460	1	-42537	-1301119
8	8	9	-1	56615	1301113	1	-56615	-1138632
9	9	10	-1	58834	1138629	1	-58834	-952125
10	10	11	-1	56215	952125	1	-56215	-665990
11	11	12	-1	47427	665990	1	-47427	-424588
12	12	13	-1	36251	424588	1	-36251	-240071
13	13	14	-1	25124	240071	1	-25124	-112188
14	14	15	-1	15449	112188	1	-15449	-32555
15	15	16	-1	7899	33555	1	-7899	6653
16	16	17	-1	2701	-6653	1	-2701	14972
17	17	18	-1	900	-14972	1	-900	17359
18	18	19	-1	-1406	-17339	1	1406	11421
19	19	20	-1	-2048	-11421	1	2048	2799
20	20	21	-1	-1064	-2799	1	1064	0
21	2	22	0	-1	2	0	1	1
22	3	23	0	-42827	8	0	42827	4
23	4	24	0	-64848	2	0	64848	1
24	6	25	0	-34861	1	0	34861	1
25	8	26	0	-14079	1	0	14079	0
26	9	27	0	-2219	3	0	2219	1
27	10	28	0	2619	0	0	-2619	0
28	11	29	0	8783	0	0	-8783	0
29	12	30	0	11176	0	0	-11176	0
30	13	31	0	11126	0	0	-11126	0
31	14	32	0	9676	0	0	-9676	0
32	15	33	0	7549	0	0	-7549	0
33	16	34	0	5198	0	0	-5198	0
34	17	35	0	1801	-1	0	-1801	0
35	18	36	0	2306	0	0	-2306	0
36	19	37	0	642	0	0	-642	0
37	20	38	0	-984	0	0	984	0
38	21	39	0	-1064	0	0	1064	0

Table D (continued)

Beam Loads and Stresses

Beam	I	J	PAX	SAX	SH1	SH2	BM1	BM2
1	1	2	0	0	100000	100000	0	-1133000
2	2	3	0	0	99999	99999	-1134993	-1362997
3	3	4	1	0	57173	57173	-1362989	-1527075
4	4	5	1	0	-7675	-7675	-1527073	-1507895
5	5	6	1	0	-7675	-7675	-1507895	-1492535
6	6	7	1	0	-42537	-42537	-1492534	-1407460
7	7	8	1	0	-42537	-42537	-1407460	-1301119
8	8	9	1	0	-56615	-56615	-1301118	-1138632
9	9	10	1	0	-58834	-58834	-1138629	-952125
10	10	11	1	0	-56215	-56215	-952125	-665990
11	11	12	1	0	-47427	-47427	-665990	-424588
12	12	13	1	0	-36251	-36251	-424588	-240071
13	13	14	1	0	-25124	-25124	-240071	-112188
14	14	15	1	0	-15449	-15449	-112188	-33535
15	15	16	1	0	-7899	-7899	-33535	6653
16	16	17	1	0	-2701	-2701	6653	14972
17	17	18	1	0	-900	-900	14972	17339
18	18	19	1	0	1406	1406	17339	11421
19	19	20	1	0	2048	2048	11421	2799
20	20	21	1	0	1064	1064	2799	0
21	2	22	-1	-347	0	0	-2	1
22	3	23	-42827	-2141	0	0	-8	4
23	4	24	-64848	-326	0	0	-2	1
24	6	25	-34861	-173	0	0	-1	1
25	8	26	-14079	-71	0	0	-1	0
26	9	27	-2219	-111	0	0	-3	1
27	10	28	2619	16	0	0	0	0
28	11	29	8788	53	0	0	0	0
29	12	30	11176	67	0	0	0	0
30	13	31	11126	67	0	0	0	0
31	14	32	9676	58	0	0	0	0
32	15	33	7549	45	0	0	0	0
33	16	34	5198	31	0	0	0	0
34	17	35	1801	109	0	0	1	0
35	18	36	2306	15	0	0	0	0
36	19	37	642	4	0	0	0	0
37	20	38	-984	-7	0	0	0	0
38	21	39	-1064	-65	0	0	0	0

Node Internal Forces and Reactions

Node	X	Y	FX	FY	MZ
1	0	0	0	0	0
2	11.35	0	0	0	0
3	13.63	0	0	0	0
4	16.5	0	0	0	0

Table D (continued)

5	19	0	0	0	0
6	21	0	0	0	0
7	23	0	0	0	0
8	25.5	0	0	0	0
9	28.37	0	0	0	0
10	31.54	0	0	0	0
11	36.63	0	0	0	0
12	41.72	0	0	0	0
13	46.81	0	0	0	0
14	51.9	0	0	0	0
15	56.99	0	0	0	0
16	62.08	0	0	0	0
17	65.16	0	0	0	0
18	67.79	0	0	0	0
19	72	0	0	0	0
20	76.21	0	0	0	0
21	78.84	0	1	0	0
22	11.35	-29	0	1	1
23	13.63	-29	0	42827	4
24	16.5	-29	0	64848	1
25	21	-29	0	34861	1
26	25.5	-29	0	14079	0
27	28.37	-29	0	2219	1
28	31.54	-29	0	-2619	0
29	36.63	-29	0	-8788	0
30	41.72	-29	0	-11176	0
31	46.81	-29	0	-11126	0
32	51.9	-29	0	-9676	0
33	56.99	-29	0	-7549	0
34	62.08	-29	0	-5198	0
35	65.16	-29	0	-1801	0
36	67.79	-29	0	-2306	0
37	72	-29	0	-642	0
38	76.21	-29	0	984	0
39	78.84	-29	0	1066	0

Table E. Foundation reaction:
inner concrete load bearing

Node	Axial Location	Length of Reaction Foundation	Foundation Load	Foundation Loading	Starting Location
		(in.)	lb.	Lb./in.	(in.)
22	11.35	3.31	44079	13316.918	9.695
23	13.63	1.25	31241	24992.8	13.005
24	16.5	4.5	45840	10186.666	14.25
25	21	4.5	22602	5022.6666	18.75
26	25.5	4.5	7026	1561.3333	23.25
27	28.37	1.25	54	43.2	27.745
28	31.54	5.09	-4454	-875.0491	28.995
29	36.63	5.09	-8537	-1677.210	34.085

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Table E (continued)

30	41.72	5.09	-9782	-1921.307	39.175
31	46.81	5.09	-9265	-1829.235	44.265
32	51.9	5.09	-7792	-1536.844	49.355
33	56.99	5.09	-5905	-1160.117	54.445
34	58.052	5.09	-3930	-772.1021	55.507
35	65.16	1.062	-1318	-1241.054	64.629
36	67.79	4.21	-1604	-380.9976	65.685
37	72	4.21	-267	-63.42042	69.995
38	73.062	4.21	1032	245.13064	70.957
39	78.844	1.062	981	923.72881	78.313
				923.72881	79.275

Foundation Reaction : Inner Concrete Not Load Bearing

Node	Axial Location	Length of Reaction Foundation (in.)	Reaction Load lb.	Foundation Loading Lb./in.	Starting Location (in.)
22	11.35	3.31		1 0.3021148	9.695
23	13.63	1.25	42827	34241.6	13.005
24	16.5	4.5	64848	14410.666	14.25
25	21	4.5	34861	7746.8888	18.75
26	25.5	4.5	14079	3128.6666	23.25
27	28.37	1.25	2219	1775.2	27.745
28	31.54	5.09	-2619	-514.5383	28.995
29	36.63	5.09	-8788	-1726.522	34.095
30	41.72	5.09	-11175	-2195.677	39.175
31	46.81	5.09	-11126	-2185.354	44.265
32	51.9	5.09	-9676	-1900.982	49.355
33	56.99	5.09	-7549	-1483.104	54.445
34	58.052	5.09	-5198	-1021.213	55.507
35	65.16	1.062	-1801	-1695.856	64.629
36	67.79	4.21	-2396	-547.7434	65.685
37	72	4.21	-642	-152.4940	69.995
38	73.062	4.21	984	233.72921	70.957
39	78.844	1.062	1064	1001.8832	78.313
				1001.8832	79.375

Appendix 2

TROJAN CRITICAL-FLAW-SIZE ANALYSIS FOR THE
BEAM-TOP FLANGE AT THE INTERSECTION
WITH THE REACTOR CAVITY LINER

T40A

Table A 2.1

TROJAN : SUPPORT BEAM CRITICAL FLAW SIZE ANALYSIS

Stress location :	Interface with the cavity liner					
Flaw type	Uniform depth surface flaw					
Flaw aspect ratio a/l	0.000					
Plate thickness in.	2.500					
Stresses in beam flange without flaw :	Unit load	Load case	Load case	Max. surface	Flange mean	Flange bend.
	(200 Kip) BM	maximum load	maximum BM	stress	stress	stress
Faulted (2) = DW + T + SB:LOCA	969000.000	558000.000	7548510.000	12380.470	10660.960	1719.510
Faulted (3) = DW + T + LB:LOCA	969000.000	2443000.000	11836335.000	19413.022	16716.769	2696.253
Flaw shape parameter = 0	Sy =	40000.000	Faulted(2)	(Sa + Sb)/Sy =	0.310	Q = 0.990
			Faulted(3)	(Sa + Sb)/Sy =	0.485	Q = 0.970
Flaw depth "a" in.	0.050	0.100	0.200	0.300	0.600	1.250
Flaw depth to flange thickness ratio	0.020	0.040	0.080	0.120	0.240	0.500
Membrane correction factor Ma	1.130	1.130	1.170	1.230	1.500	3.500
Bending correction factor Mb	1.090	1.060	1.030	1.020	1.060	1.580
Faulted condition (2)						
(a) = $Sa + Mb \sqrt{\pi} \sqrt{a/Q}$	4798.628	6786.285	9936.982	12794.384	22065.844	74315.039
(b) = $Sb + Mb \sqrt{\pi} \sqrt{a/Q}$	746.575	1026.757	1410.958	1711.287	2515.032	5410.957
KI = (a) + (b)	5545.203	7813.042	11347.941	14505.671	24580.876	79725.995
Faulted condition (3)						
(a) = $Sa + Mb \sqrt{\pi} \sqrt{a/Q}$	7601.597	10750.282	15741.360	20267.825	34954.918	117723.849
(b) = $Sb + Mb \sqrt{\pi} \sqrt{a/Q}$	1182.664	1626.504	2235.126	2710.881	3934.109	8571.598
KI = (a) + (b)	8784.261	12376.787	17976.486	22978.706	38939.027	126295.447

T41A

Table A 2.2

TROJAN : SUPPORT BEAM CRITICAL FLAW SIZE ANALYSIS

Stress location :	Interface with the cavity liner					
Flaw type	Elliptical surface flaw					
Flaw aspect ratio a/l	0.100					
Plate thickness in.	2.500					
Stresses in beam flange without flaw :	Unit load (200 Kip) BN	Load case maximum load	Load case maximum BN	Max. surface stress	Flange mean stress	Flange bend. stress
Faulted (2) = DW + T + SB:LOCA	969000.000	1558000.000	7548510.000	12380.470	10660.960	1719.510
Faulted (3) = DW + T + LB:LOCA	969000.000	2443000.000	11836335.000	19413.022	16716.769	2696.253
Flaw shape parameter = Q	Sy =	40000.000	Faulted(2)	(Sm + Sb)/Sy =	0.310	Q = 1.090
			Faulted(3)	(Sm + Sb)/Sy =	0.485	Q = 1.060
Flaw depth "a" in.	0.050	0.100	0.200	0.300	0.600	1.250
Flaw depth to flange thickness ratio	0.020	0.040	0.080	0.120	0.240	0.500
Membrane correction factor Mm	1.100	1.100	1.120	1.140	1.250	1.810
Bending correction factor Mb	1.040	1.020	0.980	0.940	0.870	0.890
Faulted condition (2)						
(a) = $S_m \cdot M_m \cdot \sqrt{PI} \cdot \sqrt{a/Q}$	4451.800	6295.797	9065.485	11301.172	17524.420	36626.177
(b) = $S_b \cdot M_b \cdot \sqrt{PI} \cdot \sqrt{a/Q}$	678.867	941.600	1279.403	1502.986	1967.257	2904.767
K1 = (a) + (b)	5130.667	7237.397	10344.888	12804.158	19491.677	39530.944
Faulted condition (3)						
(a) = $S_m \cdot M_m \cdot \sqrt{PI} \cdot \sqrt{a/Q}$	7078.676	10010.759	14414.758	17969.658	27865.059	58238.196
(b) = $S_b \cdot M_b \cdot \sqrt{PI} \cdot \sqrt{a/Q}$	1079.446	1497.210	2034.341	2389.853	3128.078	4618.784
K1 = (a) + (b)	8158.122	11507.970	16449.099	20359.511	30993.137	62856.981

T42A

Table A 2.3

TROJAN : SUPPORT BEAM CRITICAL FLAW SIZE ANALYSIS

Stress location :	Interface with the cavity liner					
Flaw type	!Elliptical surface flaw					
Flaw aspect ratio a/l	: 0.200					
Plate thickness in.	: 2.500					
Stresses in beam flange without flaw :	!Unit load	!Load case	!Load case	!Max. surface	!Flange mean	!Flange bend.
	!((200 Kip) BM	!maximum load	!maximum BM	!stress	!stress	!stress
Faulted (2) = DM + T + SB:LOCA	: 969000.000	: 1358000.000	: 7348510.000	: 12380.470	: 10660.960	: 1719.510
Faulted (3) = DM + T + LB:LOCA	: 969000.000	: 2443000.000	: 11836335.000	: 19413.022	: 16716.769	: 2696.253
Flaw shape parameter = Q	: Sy =	40000.000	!Faulted(2)	(Sa + Sb)/Sy =	0.310	Q = 1.280
			!Faulted(3)	(Sa + Sb)/Sy =	0.485	Q = 1.260
Flaw depth "a" in.	: 0.050	: 0.100	: 0.200	: 0.300	: 0.600	: 1.250
Flaw depth to flange thickness ratio	: 0.020	: 0.040	: 0.080	: 0.120	: 0.240	: 0.500
Membrane correction factor Mm	: 1.100	: 1.100	: 1.100	: 1.100	: 1.150	: 1.400
Bending correction factor Mb	: 1.030	: 1.000	: 0.950	: 0.890	: 0.770	: 0.570
Faulted condition (2)						
(a) = $S_m M_m \sqrt{2SQRT(PI) \sqrt{2SQRT(a/Q)}}$	: 4108.128	: 5809.770	: 8216.256	: 10062.817	: 14877.835	: 26142.632
(b) = $S_b M_b \sqrt{2SQRT(PI) \sqrt{2SQRT(a/Q)}}$	: 620.436	: 851.872	: 1144.493	: 1313.183	: 1606.723	: 1716.740
KI = (a) + (b)	: 4728.564	: 6661.643	: 9360.749	: 11376.000	: 16484.558	: 27859.372
Faulted condition (3)						
(a) = $S_m M_m \sqrt{2SQRT(PI) \sqrt{2SQRT(a/Q)}}$	: 6492.615	: 9181.945	: 12985.231	: 15903.595	: 23513.402	: 41316.644
(b) = $S_b M_b \sqrt{2SQRT(PI) \sqrt{2SQRT(a/Q)}}$	: 980.536	: 1346.326	: 1808.793	: 2075.396	: 2539.315	: 2713.190
KI = (a) + (b)	: 7473.172	: 10528.271	: 14794.024	: 17978.991	: 26052.717	: 44029.834

T43A

Table A 2.4

TROJAN : SUPPORT BEAM CRITICAL FLAW SIZE ANALYSIS

Stress location :	:Interface with the cavity liner					
Flaw type	:Elliptical surface flaw					
Flaw aspect ratio a/l	: 0.300					
Plate thickness in.	: 2.500					
:						
Stresses in beam flange without flaw :	:Unit load	:Load case	:Load case	:Max. surface	:Flange mean	:Flange bend. :
	:((200 Kip) BM	:maximum load	:maximum BM	:stress	:stress	:stress
:						
Faulted (2) = DW + T + SB:LOCA	: 969000.000	: 1538000.000	: 7548510.000	: 12380.470	: 10660.960	: 1719.510
Faulted (3) = DW + T + LB:LOCA	: 969000.000	: 2443000.000	: 11836335.000	: 19413.022	: 16716.769	: 2696.253
:						
:						
Flaw shape parameter = Q	: Sy =	40000.000	:Faulted(2)	(Sm + Sb)/Sy =	0.310	: Q = 1.630
			:Faulted(3)	(Sm + Sb)/Sy =	0.485	: Q = 1.610
:						
Flaw depth "a" in.	: 0.050	: 0.100	: 0.200	: 0.300	: 0.600	: 1.250
Flaw depth to flange thickness ratio	: 0.020	: 0.040	: 0.080	: 0.120	: 0.240	: 0.500
Membrane correction factor Mm	: 1.100	: 1.100	: 1.100	: 1.100	: 1.120	: 1.230
Bending correction factor Mb	: 1.010	: 0.980	: 0.920	: 0.860	: 0.720	: 0.420
:						
Faulted condition (2)	:					
:						
(a) = Sm*Mm*2SQRT(PI)*2SQRT(a/Q)	: 3640.451	: 5148.375	: 7280.901	: 8917.246	: 12840.180	: 20353.428
(b) = Sb*Mb*2SQRT(PI)*2SQRT(a/Q)	: 539.129	: 739.796	: 982.174	: 1124.462	: 1331.353	: 1120.960
KI = (a) + (b)	: 4179.579	: 5888.170	: 8263.076	: 10041.709	: 14171.535	: 21474.388
:						
Faulted condition (3)	:					
:						
(a) = Sm*Mm*2SQRT(PI)*2SQRT(a/Q)	: 5743.704	: 8122.823	: 11487.407	: 14069.143	: 20258.532	: 32112.524
(b) = Sb*Mb*2SQRT(PI)*2SQRT(a/Q)	: 850.607	: 1167.209	: 1549.621	: 1774.115	: 2100.539	: 1768.589
KI = (a) + (b)	: 6594.311	: 9290.033	: 13037.028	: 15843.258	: 22359.071	: 33881.114

T45A

Table A 2.5

TROJAN : SUPPORT BEAM CRITICAL FLAW SIZE ANALYSIS

Stress location :	:Interface with the cavity liner					
Flaw type	:Elliptical surface flaw					
Flaw aspect ratio a/l	: 0.500					
Plate thickness in.	: 2.500					
Stresses in beam flange without flaw :	:Unit load	:Load case	:Load case	:Max. surface	:Flange mean	:Flange bend.
	: (200 Kip) BM	: maximum load	: maximum BM	: stress	: stress	: stress
Faulted (2) = DW + T + SB:LOCA	: 969000.000	: 1558000.000	: 7548510.000	: 12380.470	: 10660.960	: 1719.510
Faulted (3) = DW + T + LB:LOCA	: 969000.000	: 2443000.000	: 11836335.000	: 19413.022	: 16716.769	: 2696.253
Flaw shape parameter = Q	: Sy =	40000.000	:Faulted(2)	(Sm + Sb)/Sy =	0.310	: Q = 2.440
			:Faulted(3)	(Sm + Sb)/Sy =	0.485	: Q = 2.420
Flaw depth "a" in.	: 0.050	: 0.100	: 0.200	: 0.300	: 0.600	: 1.250
Flaw depth to flange thickness ratio	: 0.020	: 0.040	: 0.080	: 0.120	: 0.240	: 0.500
Membrane correction factor Mm	: 1.100	: 1.100	: 1.100	: 1.100	: 1.100	: 1.100
Bending correction factor Mb	: 0.990	: 0.960	: 0.900	: 0.840	: 0.670	: 0.300
Faulted condition (2)						
(a) = Sm*Mm*2SQRT(PI)*2SQRT(a/Q)	: 2975.459	: 4207.935	: 5950.919	: 7288.357	: 10307.294	: 14877.297
(b) = Sb*Mb*2SQRT(PI)*2SQRT(a/Q)	: 431.922	: 592.317	: 785.312	: 897.686	: 1012.593	: 654.427
KI = (a) + (b)	: 3407.381	: 4800.254	: 6736.231	: 8186.044	: 11319.887	: 15531.724
Faulted condition (3)						
(a) = Sm*Mm*2SQRT(PI)*2SQRT(a/Q)	: 4684.867	: 6625.403	: 9369.734	: 11475.534	: 16228.856	: 23424.335
(b) = Sb*Mb*2SQRT(PI)*2SQRT(a/Q)	: 680.061	: 932.608	: 1236.475	: 1413.409	: 1594.330	: 1030.396
KI = (a) + (b)	: 5364.928	: 7558.011	: 10606.209	: 12888.943	: 17823.186	: 24454.731

T40AS

Table A 2.6

TROJAN : SUPPORT BEAM CRITICAL FLAW SIZE ANALYSIS

Stress location :	Interface with the cavity liner					
Flaw type	Uniform depth surface flaw					
Flaw aspect ratio a/l	0.000					
Plate thickness in.	2.500					
Stresses in beam flange without flaw :	Unit load (200 Kip) BM	Load case maximum load	Load case maximum BM	Max. surface stress	Flange mean stress	Flange bend. stress
Normal + Upset = DW + T + OBE	969000.000	1099000.000	5324655.000	8733.079	7520.151	1212.928
Faulted (1) = DW + T + SSE	969000.000	1212000.000	5872140.000	9631.020	8293.378	1337.642
Flaw shape parameter = 0	Sy =	40000.000	Normal+Upset Faulted(1)	(Sa + Sb)/Sy =	0.218	0 = 1.000
				(Sa + Sb)/Sy =	0.241	0 = 1.000
Flaw depth "a" in.	0.050	0.100	0.200	0.300	0.400	1.250
Flaw depth to flange thickness ratio	0.020	0.040	0.080	0.120	0.240	0.500
Membrane correction factor Ma	1.130	1.130	1.170	1.230	1.500	3.500
Bending correction factor Mb	1.090	1.060	1.030	1.020	1.060	1.580
Normal plus upset conditions						
(a) = $S_m M_m \sqrt{\pi} \sqrt{a/Q}$	3367.945	4762.993	6974.328	8979.812	15487.038	52158.434
(b) = $S_b M_b \sqrt{\pi} \sqrt{a/Q}$	523.988	720.636	990.239	1201.076	1765.189	3797.711
K1 = (a) + (b)	3891.923	5483.628	7964.567	10180.888	17252.228	55956.145
Faulted condition (1)						
(a) = $S_m M_m \sqrt{\pi} \sqrt{a/Q}$	3714.230	5252.727	7691.433	9903.123	17079.427	57521.403
(b) = $S_b M_b \sqrt{\pi} \sqrt{a/Q}$	577.865	794.732	1092.111	1324.572	1946.687	4188.194
K1 = (a) + (b)	4292.104	6047.459	8783.545	11227.695	19026.115	61709.598

T41AS

Table A 2.7

TROJAN : SUPPORT BEAM CRITICAL FLAW SIZE ANALYSIS

Stress location :	Interface with the cavity liner					
Flaw type	Elliptical surface flaw					
Flaw aspect ratio a/l	0.100					
Plate thickness in.	2.500					
Stresses in beam flange without flaw :	Unit load (200 Kip) BM	Load case maximum load	Load case maximum BM	Max. surface stress	Flange mean stress	Flange bend. stress
Normal + Upset = DW + T + OBE	969000.000	1099000.000	5324655.000	8733.079	7520.151	1212.928
Faulted (I) = DW + T + SSE	969000.000	1212000.000	5872140.000	9631.020	8293.378	1337.642
Flaw shape parameter = 0	Sy =	40000.000	Normal+Upset	(Sm + Sb)/Sy =	0.218	Q = 1.100
			Faulted(I)	(Sm + Sb)/Sy =	0.241	Q = 1.100
Flaw depth "a" in.	0.050	0.100	0.200	0.300	0.600	1.250
Flaw depth to flange thickness ratio	0.020	0.040	0.080	0.120	0.240	0.500
Membrane correction factor Mm	1.100	1.100	1.120	1.140	1.250	1.810
Bending correction factor Mb	1.040	1.020	0.980	0.940	0.870	0.890
Normal plus upset conditions						
(a) = $S_m M_m \sqrt{\pi} \sqrt{a/Q}$	3125.956	4420.769	6365.583	7935.433	12305.260	25718.091
(b) = $S_b M_b \sqrt{\pi} \sqrt{a/Q}$	476.685	661.171	898.369	1055.363	1381.365	2039.663
KI = (a) + (b)	3602.641	5081.940	7263.951	8990.796	13686.625	27757.754
Faulted condition (I)						
(a) = $S_m M_m \sqrt{\pi} \sqrt{a/Q}$	3447.369	4875.316	7020.097	8751.360	13579.496	28362.445
(b) = $S_b M_b \sqrt{\pi} \sqrt{a/Q}$	525.698	729.153	990.739	1163.876	1523.398	2249.383
KI = (a) + (b)	3973.067	5604.469	8010.836	9915.237	15093.893	30611.828

T42AS

Table A 2.8

TROJAN : SUPPORT BEAM CRITICAL FLAW SIZE ANALYSIS

Stress location :	:Interface with the cavity liner					
Flaw type	:Elliptical surface flaw					
Flaw aspect ratio a/l	: 0.200					
Plate thickness in.	: 2.500					
Stresses in beam flange without flaw :	:Unit load	:Load case	:Load case	:Max. surface	:Flange mean	:Flange bend. :
	:(200 Kip) BM	:maximum load	:maximum BM	:stress	:stress	:stress
Normal + Upset = DW + T + OBE	: 969000.000	: 1099000.000	: 5324655.000	: 8733.079	: 7520.151	: 1212.928
Faulted (1) = DW + T + SSE	: 969000.000	: 1212000.000	: 5872140.000	: 9631.020	: 8293.378	: 1337.642
Flaw shape parameter = Q	: Sy =	40000.000	:Normal+Upset	(Sa + Sb)/Sy =	0.218	: Q = 1.290
			:Faulted(1)	(Sa + Sb)/Sy =	0.241	: Q = 1.290
Flaw depth "a" in.	: 0.050	: 0.100	: 0.200	: 0.300	: 0.600	: 1.250
Flaw depth to flange thickness ratio	: 0.020	: 0.040	: 0.080	: 0.120	: 0.240	: 0.500
Membrane correction factor Mm	: 1.100	: 1.100	: 1.100	: 1.100	: 1.150	: 1.400
Bending correction factor Mb	: 1.030	: 1.000	: 0.950	: 0.890	: 0.770	: 0.570
Normal plus upset conditions						
(a) = Sm*Mm*2SQRT(PI)*2SQRT(a/Q)	: 2886.585	: 4082.247	: 5773.170	: 7070.660	: 10453.942	: 18369.176
(b) = Sb*Mb*2SQRT(PI)*2SQRT(a/Q)	: 435.950	: 598.570	: 804.181	: 922.711	: 1128.967	: 1206.271
K1 = (a) + (b)	: 3322.535	: 4680.817	: 6577.350	: 7993.371	: 11582.910	: 19575.447
Faulted condition (1)						
(a) = Sm*Mm*2SQRT(PI)*2SQRT(a/Q)	: 3183.386	: 4501.987	: 6366.771	: 7797.670	: 11528.825	: 20257.909
(b) = Sb*Mb*2SQRT(PI)*2SQRT(a/Q)	: 480.775	: 660.115	: 886.867	: 1017.585	: 1245.048	: 1330.300
K1 = (a) + (b)	: 3664.161	: 5162.103	: 7253.638	: 8815.255	: 12773.873	: 21588.209

T43AS

Table A 2.9

TROJAN : SUPPORT BEAM CRITICAL FLAW SIZE ANALYSIS

Stress location :	Interface with the cavity liner					
Flaw type	Elliptical surface flaw					
Flaw aspect ratio a/l	0.300					
Plate thickness in.	2.500					
Stresses in beam flange without flaw :	Unit load (200 Kip) BM	Load case maximum load	Load case maximum BM	Max. surface stress	Flange mean stress	Flange bend. stress
Normal + Upset = DW + T + OBE	969000.000	1099000.000	5324655.000	8733.079	7520.151	1212.928
Faulted (I) = DW + T + SSE	969000.000	1212000.000	5872140.000	9631.020	8293.378	1337.642
Flaw shape parameter = Q	Sy =	40000.000	Normal+Upset	(Sa + Sb)/Sy =	0.218	Q = 1.640
			Faulted(I)	(Sa + Sb)/Sy =	0.241	Q = 1.650
Flaw depth "a" in.	0.050	0.100	0.200	0.300	0.600	1.250
Flaw depth to flange thickness ratio	0.020	0.040	0.080	0.120	0.240	0.500
Membrane correction factor Mm	1.100	1.100	1.100	1.100	1.120	1.230
Bending correction factor Mb	1.010	0.980	0.920	0.860	0.720	0.420
Normal plus upset conditions						
(a) = $S_m * M_m * \sqrt{PI} * \sqrt{a/Q}$	2560.102	3620.531	5120.204	6270.943	9029.698	14313.297
(b) = $S_b * M_b * \sqrt{PI} * \sqrt{a/Q}$	379.135	520.252	690.702	790.764	936.259	788.301
KI = (a) + (b)	2939.237	4140.783	5810.906	7061.708	9965.957	15101.598
Faulted condition (I)						
(a) = $S_m * M_m * \sqrt{PI} * \sqrt{a/Q}$	2814.765	3980.679	5629.530	6894.738	9927.916	15737.095
(b) = $S_b * M_b * \sqrt{PI} * \sqrt{a/Q}$	416.849	572.004	759.409	869.424	1029.392	866.716
KI = (a) + (b)	3231.614	4552.682	6388.939	7764.162	10957.308	16603.812

T45AS

Table A 2.10

TROJAN : SUPPORT BEAM CRITICAL FLAW SIZE ANALYSIS

Stress location :	Interface with the cavity liner					
Flaw type	Elliptical surface flaw					
Flaw aspect ratio a/l	0.500					
Plate thickness in.	2.500					
Stresses in beam flange without flaw :	Unit load (200 Kip) BW	Load case maximum load	Load case maximum BM	Max. surface stress	Flange mean stress	Flange bend. stress
Normal + Upset = DW + T + OBE	969000.000	1099000.000	5324655.000	8733.079	7520.151	1212.928
Faulted (1) = DW + T + SSE	969000.000	1212000.000	5872140.000	9631.020	8293.378	1337.642
Flaw shape parameter = Q	Sy = 40000.000	Normal+Upset	(Sm + Sb)/Sy =	0.218	Q = 2.450	
		Faulted(1)	(Sm + Sb)/Sy =	0.241	Q = 2.410	
Flaw depth "a" in.	0.050	0.100	0.200	0.300	0.600	1.250
Flaw depth to flange thickness ratio	0.020	0.040	0.080	0.120	0.240	0.500
Membrane correction factor Mm	1.100	1.100	1.100	1.100	1.100	1.100
Bending correction factor Mb	0.990	0.960	0.900	0.840	0.670	0.300
Normal plus upset conditions						
(a) = $S_m + M_m * 2SQRT(PI) * 2SQRT(a/Q)$	2094.576	2962.178	4189.152	5130.643	7255.824	10472.880
(b) = $S_b + M_b * 2SQRT(PI) * 2SQRT(a/Q)$	304.051	416.963	552.821	631.927	712.816	460.684
KI = (a) + (b)	2398.627	3379.141	4741.973	5762.569	7968.640	10933.564
Faulted condition (1)						
(a) = $S_m + M_m * 2SQRT(PI) * 2SQRT(a/Q)$	2329.033	3293.750	4658.065	5704.942	8068.006	11645.164
(b) = $S_b + M_b * 2SQRT(PI) * 2SQRT(a/Q)$	338.085	463.636	614.701	702.661	792.605	512.251
KI = (a) + (b)	2667.118	3757.386	5272.766	6407.603	8860.611	12157.414

Table A 2.11

TROJAN: SUPPORT BEAM CRITICAL FLAW SIZE ANALYSIS

STRESS INTENSITY SUMMARY : INTERFACE WITH THE CAVITY LINER

Load case	Inner Concrete Effective ?	Flaw Aspect Ratio	-----Flaw Depth (in.)-----						
			0.00	0.05	0.10	0.20	0.30	0.60	1.25
DW+T+OBE	No	0.00	0.00	3891.00	5483.00	7964.00	10190.00	17252.00	55956.00
DW+T+OBE	No	0.10	0.00	3602.00	5081.00	7263.00	8990.00	13686.00	27757.00
DW+T+OBE	No	0.20	0.00	3322.00	4680.00	6577.00	7993.00	11582.00	19575.00
DW+T+OBE	No	0.30	0.00	2939.00	4140.00	5810.00	7061.00	9965.00	15101.00
DW+T+OBE	No	0.50	0.00	2393.00	3379.00	4741.00	5762.00	7968.00	10933.00
DW+T+OBE	Yes	0.00	0.00	N.A.	N.A.	N.A.	N.A.	N.A.	N.A.
DW+T+OBE	Yes	0.10	0.00	N.A.	N.A.	N.A.	N.A.	N.A.	N.A.
DW+T+OBE	Yes	0.20	0.00	N.A.	N.A.	N.A.	N.A.	N.A.	N.A.
DW+T+OBE	Yes	0.30	0.00	N.A.	N.A.	N.A.	N.A.	N.A.	N.A.
DW+T+OBE	Yes	0.50	0.00	N.A.	N.A.	N.A.	N.A.	N.A.	N.A.
DW+T+SSE	No	0.00	0.00	4292.00	6047.00	8783.00	11227.00	19026.00	61709.00
DW+T+SSE	No	0.10	0.00	3973.00	5604.00	8010.00	9915.00	15093.00	30611.00
DW+T+SSE	No	0.20	0.00	3664.00	5162.00	7253.00	8815.00	12773.00	21598.00
DW+T+SSE	No	0.30	0.00	3231.00	4552.00	6388.00	7764.00	10957.00	16603.00
DW+T+SSE	No	0.50	0.00	2667.00	3757.00	5272.00	6407.00	8860.00	12517.00
DW+T+SSE	Yes	0.00	0.00	N.A.	N.A.	N.A.	N.A.	N.A.	N.A.
DW+T+SSE	Yes	0.10	0.00	N.A.	N.A.	N.A.	N.A.	N.A.	N.A.
DW+T+SSE	Yes	0.20	0.00	N.A.	N.A.	N.A.	N.A.	N.A.	N.A.
DW+T+SSE	Yes	0.30	0.00	N.A.	N.A.	N.A.	N.A.	N.A.	N.A.
DW+T+SSE	Yes	0.50	0.00	N.A.	N.A.	N.A.	N.A.	N.A.	N.A.
DW+T+SB:LOCA	No	0.00	0.00	5545.20	7813.04	11347.94	14505.67	24580.88	79726.00
DW+T+SB:LOCA	No	0.10	0.00	5130.67	7237.40	10344.89	12804.16	19491.68	39530.94
DW+T+SB:LOCA	No	0.20	0.00	4728.56	6661.64	9360.75	11376.00	16484.56	27859.37
DW+T+SB:LOCA	No	0.30	0.00	4179.58	5888.17	8263.08	10041.71	14171.53	21474.39
DW+T+SB:LOCA	No	0.50	0.00	3407.38	4800.25	6736.23	8186.04	11319.89	15531.72
DW+T+SB:LOCA	Yes	0.00	0.00	N.A.	N.A.	N.A.	N.A.	N.A.	N.A.
DW+T+SB:LOCA	Yes	0.10	0.00	N.A.	N.A.	N.A.	N.A.	N.A.	N.A.
DW+T+SB:LOCA	Yes	0.20	0.00	N.A.	N.A.	N.A.	N.A.	N.A.	N.A.
DW+T+SB:LOCA	Yes	0.30	0.00	N.A.	N.A.	N.A.	N.A.	N.A.	N.A.
DW+T+SB:LOCA	Yes	0.50	0.00	N.A.	N.A.	N.A.	N.A.	N.A.	N.A.
DW+T+LB:LOCA	No	0.00	0.00	8784.00	12376.00	17976.00	22978.00	38939.00	126295.00
DW+T+LB:LOCA	No	0.10	0.00	8158.00	11507.00	16449.00	20359.00	30993.00	62856.00
DW+T+LB:LOCA	No	0.20	0.00	7473.00	10528.00	14794.00	17978.00	26052.00	44029.00
DW+T+LB:LOCA	No	0.30	0.00	6594.00	9290.00	13037.00	15843.00	22359.00	33881.00
DW+T+LB:LOCA	No	0.50	0.00	5364.00	7558.00	10606.00	12888.00	17823.00	24454.00
DW+T+LB:LOCA	Yes	0.00	0.00	N.A.	N.A.	N.A.	N.A.	N.A.	N.A.
DW+T+LB:LOCA	Yes	0.10	0.00	N.A.	N.A.	N.A.	N.A.	N.A.	N.A.
DW+T+LB:LOCA	Yes	0.20	0.00	N.A.	N.A.	N.A.	N.A.	N.A.	N.A.
DW+T+LB:LOCA	Yes	0.30	0.00	N.A.	N.A.	N.A.	N.A.	N.A.	N.A.
DW+T+LB:LOCA	Yes	0.50	0.00	N.A.	N.A.	N.A.	N.A.	N.A.	N.A.

DW+T+OBE = 1099 Kips.

DW+T+SSE = 1212 Kips

DW+T+SB:LOCA= 1558 Kips.

DW+T+LB:LOCA= 2443 Kips.

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Appendix 3

**TROJAN CRITICAL-FLAW-SIZE-ANALYSIS FOR THE
BEAM-TOP FLANGE AT THE PEAK BENDING
MOMENT LOCATION (NODE 4)**

TROJAN : SUPPORT BEAM CRITICAL FLAW SIZE ANALYSIS

Stress location :	:Node 4: Max. P4: Inner concrete not effective					
Flaw type	:Uniform depth surface flaw					
Flaw aspect ratio a/l	: 0.000					
Plate thickness in.	: 2.500					
Stresses in beam flange without flaw :	:Unit load	:Load case	:Load case	:Max. surface	:Flange mean	:Flange bend. :
	:(200 Kip) BM	:maximum load	:maximum BM	:stress	:stress	:stress
	:	:	:	:	:	:
Faulted (2) = DW + T + SB:LOCA	: 1527000.000	: 1558000.000	: 11895330.000	: 19509.781	: 16800.089	: 2709.692
Faulted (3) = DW + T + LB:LOCA	: 1527000.000	: 2443000.000	: 18652305.000	: 30592.037	: 26343.143	: 4248.894
Flaw shape parameter = Q	: Sy =	40000.000	:Faulted(2)	(Sm + Sb)/Sy =	0.488	: Q = 0.920
			:Faulted(3)	(Se + Sb)/Sy =	0.765	: Q = 0.870
Flaw depth "a" in.	: 0.050	: 0.100	: 0.200	: 0.300	: 0.600	: 1.250
Flaw depth to flange thickness ratio	: 0.020	: 0.040	: 0.080	: 0.120	: 0.240	: 0.500
Membrane correction factor Mm	: 1.130	: 1.130	: 1.170	: 1.230	: 1.500	: 3.500
Bending correction factor Mb	: 1.090	: 1.060	: 1.030	: 1.020	: 1.060	: 1.580
Faulted condition (2)						
(a) = Sm*Mm*SQRT(PI)*SQRT(a/Q)	: 7844.333	: 11093.563	: 16244.018	: 20915.023	: 36071.108	: 121483.040
(b) = Sb*Mb*SQRT(PI)*SQRT(a/Q)	: 1220.429	: 1678.444	: 2306.498	: 2797.446	: 4111.331	: 8845.309
K1 = (a) + (b)	: 9064.762	: 12772.006	: 18550.516	: 23712.469	: 40182.439	: 130328.349
Faulted condition (3)						
(a) = Sm*Mm*SQRT(PI)*SQRT(a/Q)	: 12648.713	: 17887.982	: 26192.911	: 33724.742	: 58163.400	: 195887.152
(b) = Sb*Mb*SQRT(PI)*SQRT(a/Q)	: 1967.899	: 2706.432	: 3719.148	: 4510.784	: 6629.377	: 14262.751
K1 = (a) + (b)	: 14616.612	: 20594.414	: 29912.058	: 38235.526	: 64792.777	: 210149.904

TROJAN : SUPPORT BEAM CRITICAL FLAW SIZE ANALYSIS

Stress location :	:Node 4: Max. BM: Inner concrete not effective						
Flaw type	:Elliptical surface flaw						
Flaw aspect ratio a/l	: 0.100						
Plate thickness in.	: 2.500						
Stresses in beam flange without flaw :	:Unit load	:Load case	:Load case	:Max. surface	:Flange mean	:Flange bend.	:
	:(200 Kip) BM	:maximum load	:maximum BM	:stress	:stress	:stress	:
	:	:	:	:	:	:	:
Faulted (2) = DW + T + SB:LOCA	: 1527000.000	: 1558000.000	: 11895330.000	: 19509.781	: 16800.089	: 2709.692	:
Faulted (3) = DW + T + LB:LOCA	: 1527000.000	: 2443000.000	: 18652305.000	: 30592.037	: 26343.143	: 4248.894	:
Flaw shape parameter = Q	: Sy =	40000.000	:Faulted(2)	(Sa + Sb)/Sy =	0.488	: Q =	1.010
			:Faulted(3)	(Sa + Sb)/Sy =	0.765	: Q =	0.970
Flaw depth "a" in.	: 0.050	: 0.100	: 0.200	: 0.300	: 0.600	: 1.250	:
Flaw depth to flange thickness ratio	: 0.020	: 0.040	: 0.080	: 0.120	: 0.240	: 0.500	:
Membrane correction factor Mm	: 1.100	: 1.100	: 1.120	: 1.140	: 1.250	: 1.810	:
Bending correction factor Mb	: 1.040	: 1.020	: 0.980	: 0.940	: 0.870	: 0.890	:
Faulted condition (2)							
(a) = $S_m M_m \sqrt{PI} \sqrt{a/Q}$	: 7287.919	: 10306.673	: 14840.852	: 18500.834	: 28688.739	: 59959.694	:
(b) = $S_b M_b \sqrt{PI} \sqrt{a/Q}$	: 1111.354	: 1541.467	: 2094.475	: 2460.496	: 3220.542	: 4755.313	:
K1 = (a) + (b)	: 8399.273	: 11848.141	: 16935.328	: 20961.330	: 31909.282	: 64715.008	:
Faulted condition (3)							
(a) = $S_m M_m \sqrt{PI} \sqrt{a/Q}$	: 11660.961	: 16491.089	: 23745.957	: 29602.074	: 45903.130	: 95937.908	:
(b) = $S_b M_b \sqrt{PI} \sqrt{a/Q}$	: 1778.211	: 2466.409	: 3351.244	: 3936.892	: 5152.997	: 7608.692	:
K1 = (a) + (b)	: 13439.172	: 18957.499	: 27097.201	: 33538.966	: 51056.126	: 103546.599	:

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Table A 2.3

TROJAN : SUPPORT BEAM CRITICAL FLAW SIZE ANALYSIS

Stress location :	!Node 41: Max. BM: Inner concrete not effective					
Flaw type	!Elliptical surface flaw					
Flaw aspect ratio a/l	! 0.200					
Plate thickness in.	! 2.500					
Stresses in beam flange without flaw :	!Unit load	!Load case	!Load case	!Max. surface	!Flange mean	!Flange bend. :
	!(200 Kip) BM	!maximum load	!maximum BM	!stress	!stress	!stress :
Faulted (2) = DW + T + SB:LOCA	! 1527000.000	! 1558000.000	! 11895330.000	! 19509.781	! 16800.089	! 2709.692 :
Faulted (3) = DW + T + LB:LOCA	! 1527000.000	! 2443000.000	! 18652305.000	! 30592.037	! 26343.143	! 4248.894 :
Flaw shape parameter = Q	Sy =	40000.000	!Faulted(2)	(Sm + Sb)/Sy =	0.488	Q = 1.230
			!Faulted(3)	(Sm + Sb)/Sy =	0.765	Q = 1.200
Flaw depth "a" in.	! 0.050	! 0.100	! 0.200	! 0.300	! 0.600	! 1.250 :
Flaw depth to flange thickness ratio	! 0.020	! 0.040	! 0.080	! 0.120	! 0.240	! 0.500 :
Membrane correction factor Mm	! 1.100	! 1.100	! 1.100	! 1.100	! 1.150	! 1.400 :
Bending correction factor Mb	! 1.030	! 1.000	! 0.950	! 0.890	! 0.770	! 0.570 :
Faulted condition (2)						
(a) = Sm*Mm*SQRT(PI)*SQRT(a/Q)	! 6604.070	! 9339.565	! 13208.139	! 16176.601	! 23917.040	! 42025.898 :
(b) = Sb*Mb*SQRT(PI)*SQRT(a/Q)	! 997.389	! 1369.438	! 1839.843	! 2111.023	! 2582.906	! 2759.765 :
K1 = (a) + (b)	! 7601.458	! 10709.003	! 15047.983	! 18287.624	! 26499.946	! 44785.663 :
Faulted condition (3)						
(a) = Sm*Mm*SQRT(PI)*SQRT(a/Q)	! 10484.062	! 14826.703	! 20968.125	! 25680.603	! 37968.669	! 66716.760 :
(b) = Sb*Mb*SQRT(PI)*SQRT(a/Q)	! 1583.379	! 2174.003	! 2920.780	! 3351.281	! 4100.403	! 4381.170 :
K1 = (a) + (b)	! 12067.432	! 17000.706	! 23888.904	! 29031.884	! 42069.072	! 71097.930 :

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Table A 3.4

TROJAN : SUPPORT BEAM CRITICAL FLAW SIZE ANALYSIS

Stress location :	:Node 4: Max. BM: Inner concrete not effective					
Flaw type	:Elliptical surface flaw					
Flaw aspect ratio a/l	: 0.300					
Plate thickness in.	: 2.500					
Stresses in beam flange without flaw :	:Unit load	:Load case	:Load case	:Max. surface	:Flange mean	:Flange bend. :
	:(200 Kip) BM	:maximum load	:maximum BM	:stress	:stress	:stress :
Faulted (2) = DW + T + SB:LOCA	: 1527000.000	: 1558000.000	: 11895330.000	: 19509.781	: 16800.089	: 2709.692 :
Faulted (3) = DW + T + LB:LOCA	: 1527000.000	: 2443000.000	: 18652305.000	: 30592.037	: 26343.143	: 4248.894 :
Flaw shape parameter = Q	: Sy =	40000.000	:Faulted(2)	(S _m + S _b)/S _y =	0.488	: Q = 1.560
			:Faulted(3)	(S _m + S _b)/S _y =	0.765	: Q = 1.520
Flaw depth "a" in.	: 0.050	: 0.100	: 0.200	: 0.300	: 0.600	: 1.250 :
Flaw depth to flange thickness ratio	: 0.020	: 0.040	: 0.080	: 0.120	: 0.240	: 0.500 :
Membrane correction factor M _a	: 1.100	: 1.100	: 1.100	: 1.100	: 1.120	: 1.230 :
Bending correction factor M _b	: 1.010	: 0.980	: 0.920	: 0.860	: 0.720	: 0.420 :
Faulted condition (2)						
(a) = S _m *M _m *2SQRT(PI)*2SQRT(a/Q)	: 5864.107	: 8293.100	: 11728.214	: 14364.070	: 20683.206	: 32785.690 :
(b) = S _b *M _b *2SQRT(PI)*2SQRT(a/Q)	: 868.438	: 1191.677	: 1582.105	: 1811.305	: 2144.572	: 1805.664 :
K ₁ = (a) + (b)	: 6732.545	: 9484.777	: 13310.320	: 16175.376	: 22827.778	: 34591.354 :
Faulted condition (3)						
(a) = S _m *M _m *2SQRT(PI)*2SQRT(a/Q)	: 9315.334	: 13173.871	: 18630.668	: 22317.815	: 32855.977	: 52081.184 :
(b) = S _b *M _b *2SQRT(PI)*2SQRT(a/Q)	: 1379.544	: 1893.020	: 2513.228	: 2877.320	: 3406.726	: 2868.358 :
K ₁ = (a) + (b)	: 10694.877	: 15066.891	: 21143.895	: 25695.134	: 36262.702	: 54949.542 :

T45B

Table A 2.5

TROJAN : SUPPORT BEAM CRITICAL FLAW SIZE ANALYSIS

Stress location :	!Mode 4: Max. 5M: Inner concrete not effective					
Flaw type	!Elliptical surface flaw					
Flaw aspect ratio a/l	: 0.500					
Plate thickness in.	: 2.500					
Stresses in beam flange without flaw :	!Unit load	!Load case	!Load case	!Max. surface	!Flange near	!Flange bend.
	!(200 Kip) BM	!maximum load	!maximum BM	!stress	!stress	!stress
	:	:	:	:	:	:
Faulted (2) = DW + T + SB:LOCA	: 1527000.000	: 1558000.000	: 11895330.000	: 19509.781	: 16800.089	: 2709.692
Faulted (3) = DW + T + LB:LOCA	: 1527000.000	: 2443000.000	: 18652305.000	: 30592.037	: 26343.143	: 4248.894
	:	:	:	:	:	:
Flaw shape parameter = Q	Sy =	40000.000	!Faulted(2)	(Sm + Sb)/Sy =	0.488	Q = 2.340
			!Faulted(3)	(Sm + Sb)/Sy =	0.765	Q = 2.310
	:	:	:	:	:	:
Flaw depth "a" in.	: 0.050	: 0.100	: 0.200	: 0.300	: 0.600	: 1.250
Flaw depth to flange thickness ratio	: 0.020	: 0.040	: 0.080	: 0.120	: 0.240	: 0.500
Membrane correction factor Mm	: 1.100	: 1.100	: 1.100	: 1.100	: 1.100	: 1.100
Bending correction factor Mb	: 0.990	: 0.960	: 0.900	: 0.840	: 0.670	: 0.300
	:	:	:	:	:	:
Faulted condition (2)	:	:	:	:	:	:
(a) = Sm*Mm*2SQRT(PI)*2SQRT(a/Q)	: 4788.023	: 6771.288	: 9576.047	: 11728.214	: 16586.200	: 23940.117
(b) = Sb*Mb*2SQRT(PI)*2SQRT(a/Q)	: 695.034	: 953.143	: 1263.701	: 1444.531	: 1629.436	: 1053.084
K1 = (a) + (b)	: 5483.059	: 7724.431	: 10839.748	: 13172.745	: 18215.636	: 24993.202
	:	:	:	:	:	:
Faulted condition (3)	:	:	:	:	:	:
(a) = Sm*Mm*2SQRT(PI)*2SQRT(a/Q)	: 7556.388	: 10686.344	: 15112.775	: 18509.294	: 26176.094	: 37781.938
(b) = Sb*Mb*2SQRT(PI)*2SQRT(a/Q)	: 1096.895	: 1504.236	: 1994.354	: 2279.737	: 2571.552	: 1661.962
K1 = (a) + (b)	: 8653.283	: 12190.582	: 17107.130	: 20789.031	: 28747.646	: 39443.900

T40C

Table A 3.6

TROJAN : SUPPORT BEAM CRITICAL FLAW SIZE ANALYSIS

Stress location :	!Node 4: Max. E4: Inner concrete effective					
Flaw type	!Uniform depth surface flaw					
Flaw aspect ratio a/l	! 0.000					
Plate thickness in.	! 2.500					
Stresses in beam flange without flaw :	!Unit load	!Load case	!Load case	!Max. surface	!Flange mean	!Flange bend. :
	!(200 Kip) BM	!maximum load	!maximum BM	!stress	!stress	!stress :
Faulted (2) = DW + T + SB:LOCA	: 1333000.000	: 1558000.000	: 10384070.000	: 17031.131	: 14665.696	: 2365.435 :
Faulted (3) = DW + T + LB:LOCA	: 1333000.000	: 2443000.000	: 16282595.000	: 26705.426	: 22996.339	: 3709.087 :
Flaw shape parameter = Q	Sy =	40000.000	!Faulted(2)	(Sm + Sb)/Sy =	0.426	Q = 0.950
			!Faulted(3)	(Sm + Sb)/Sy =	0.668	Q = 0.910
Flaw depth "a" in.	: 0.050	: 0.100	: 0.200	: 0.300	: 0.600	: 1.250 :
Flaw depth to flange thickness ratio	: 0.020	: 0.040	: 0.080	: 0.120	: 0.240	: 0.500 :
Membrane correction factor Me	: 1.130	: 1.130	: 1.170	: 1.230	: 1.500	: 3.500 :
Bending correction factor Mb	: 1.090	: 1.060	: 1.030	: 1.020	: 1.060	: 1.580 :
Faulted condition (2)						
(a) = Sm*Mb*QSQRT(PI)*QSQRT(a/Q)	: 6738.749	: 9530.030	: 13954.577	: 17967.248	: 30987.227	: 104361.155 :
(b) = Sb*Mb*QSQRT(PI)*QSQRT(a/Q)	: 1048.421	: 1441.883	: 1981.419	: 2403.172	: 3531.877	: 7598.646 :
K1 = (a) + (b)	: 7787.170	: 10971.913	: 15935.996	: 20370.420	: 34519.104	: 111959.801 :
Faulted condition (3)						
(a) = Sm*Mb*QSQRT(PI)*QSQRT(a/Q)	: 10796.336	: 15268.325	: 22357.014	: 28785.825	: 49645.493	: 167199.893 :
(b) = Sb*Mb*QSQRT(PI)*QSQRT(a/Q)	: 1679.704	: 2310.081	: 3174.486	: 3850.187	: 5658.519	: 12174.001 :
K1 = (a) + (b)	: 12476.040	: 17578.405	: 25531.501	: 32636.014	: 55304.012	: 179373.894 :

T41C

Table A 3.7

TROJAN : SUPPORT BEAM CRITICAL FLAW SIZE ANALYSIS

Stress location :	:Mode 4: Max. BM: Inner concrete effective					
Flaw type	:Elliptical surface flaw					
Flaw aspect ratio a/l	: 0.100					
Plate thickness in.	: 2.500					
Stresses in beam flange without flaw :	:Unit load	:Load case	:Load case	:Max. surface	:Flange mean	:Flange bend. :
	:(200 Kip) BM	:maximum load	:maximum BM	:stress	:stress	:stress :
Faulted (2)= DW + T + SB:LOCA	: 1333000.000	: 1558000.000	:10384070.000	: 17031.131	: 14665.696	: 2365.435 :
Faulted (3)= DW + T + LB:LOCA	: 1333000.000	: 2443000.000	:16282595.000	: 26705.426	: 22996.339	: 3709.087 :
Flaw shape parameter = Q	: Sy =	40000.000	:Faulted(2)	(S _m + S _b)/Sy =	0.426	: Q = 1.050
			:Faulted(3)	(S _m + S _b)/Sy =	0.668	: Q = 1.030
Flaw depth "a" in.	: 0.050	: 0.100	: 0.200	: 0.300	: 0.600	: 1.250 :
Flaw depth to flange thickness ratio	: 0.020	: 0.040	: 0.080	: 0.120	: 0.240	: 0.500 :
Membrane correction factor M _a	: 1.100	: 1.100	: 1.120	: 1.140	: 1.250	: 1.810 :
Bending correction factor M _b	: 1.040	: 1.020	: 0.980	: 0.940	: 0.870	: 0.890 :
Faulted condition (2)						
(a) = S _m *M _a *√(PI)*√SQRT(a/Q)	: 6239.656	: 8824.206	: 12706.209	: 15839.755	: 24562.276	: 51335.354 :
(b) = S _b *M _b *√(PI)*√SQRT(a/Q)	: 951.502	: 1319.749	: 1793.215	: 2106.589	: 2757.314	: 4071.330 :
K _I = (a) + (b)	: 7191.158	: 10143.956	: 14499.424	: 17946.344	: 27319.590	: 55406.684 :
Faulted condition (3)						
(a) = S _m *M _a *√(PI)*√SQRT(a/Q)	: 9878.539	: 13970.364	: 20116.297	: 25077.284	: 38886.662	: 81273.434 :
(b) = S _b *M _b *√(PI)*√SQRT(a/Q)	: 1506.405	: 2089.409	: 2838.994	: 3335.123	: 4365.341	: 6445.674 :
K _I = (a) + (b)	: 11384.944	: 16059.773	: 22955.291	: 28412.407	: 43252.004	: 87719.108 :

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Table A 3.8

TROJAN : SUPPORT BEAM CRITICAL FLAW SIZE ANALYSIS

Stress location :	:Node 41: Max. BM: Inner concrete effective					
Flaw type	:Elliptical surface flaw					
Flaw aspect ratio a/l	: 0.200					
Plate thickness in.	: 2.500					
Stresses in beam flange without flaw :	Unit load (200 Kip) B"	Load case maximum load	Load case maximum BM	Max. surface stress	Flange mean stress	Flange bend. stress
Faulted (2)= DW + T + SB:LOCA	1333000.000	1558000.000	10384070.000	17031.131	14665.696	2365.435
Faulted (3)= DW + T + LB:LOCA	1333000.000	2443000.000	16282595.000	26705.426	22996.339	3709.087
Flaw shape parameter = Q	Sy = 40000.000	Faulted(2)	(Sm + Sb)/Sy =	0.426	Q = 1.270	
		Faulted(3)	(Sm + Sb)/Sy =	0.668	Q = 1.240	
Flaw depth "a" in.	0.050	0.100	0.200	0.300	0.600	1.250
Flaw depth to flange thickness ratio	0.020	0.040	0.080	0.120	0.240	0.500
Membrane correction factor Mm	1.100	1.100	1.100	1.100	1.150	1.400
Bending correction factor Mb	1.020	1.000	0.950	0.890	0.770	0.570
Faulted condition (2)						
(a) = $S_m M_m \sqrt{PI} \sqrt{a/Q}$	5673.531	8023.585	11347.063	13897.257	20547.038	36104.290
(b) = $S_b M_b \sqrt{PI} \sqrt{a/Q}$	856.853	1176.479	1580.603	1813.572	2218.965	2370.904
KI = (a) + (b)	6530.384	9200.064	12927.665	15710.828	22766.003	38475.194
Faulted condition (3)						
(a) = $S_m M_m \sqrt{PI} \sqrt{a/Q}$	9003.274	12732.553	18006.549	22053.428	32605.905	57293.564
(b) = $S_b M_b \sqrt{PI} \sqrt{a/Q}$	1359.732	1866.943	2508.244	2877.940	3521.255	3762.365
KI = (a) + (b)	10363.006	14599.496	20514.792	24931.368	36127.160	61055.930

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Table A 3.9

TROJAN : SUPPORT BEAM CRITICAL FLAW SIZE ANALYSIS

Stress location :	:Node 4: Max. BM: Inner concrete effective					
Flaw type	:Elliptical surface flaw					
Flaw aspect ratio a/l	: 0.300					
Plate thickness in.	: 2.500					
Stresses in beam flange without flaw :	Unit load	Load case	Load case	Max. surface	Flange mean	Flange bend.
	:(200 Kip) BM	:maximum load	:maximum BM	:stress	:stress	:stress
Faulted (2) = DW + T + SB:LOCA	: 1333000.000	: 1558000.000	: 10384070.000	: 17031.131	: 14665.696	: 2365.435
Faulted (3) = DW + T + LB:LOCA	: 1333000.000	: 2443000.000	: 16282595.000	: 26705.426	: 22996.339	: 3709.087
Flaw shape parameter = Q	Sy =	40000.000	:Faulted(2)	(Sm + Sb)/Sy =	0.426	Q = 1.590
			:Faulted(3)	(Sm + Sb)/Sy =	0.668	Q = 1.580
Flaw depth "a" in.	: 0.050	: 0.100	: 0.200	: 0.300	: 0.600	: 1.250
Flaw depth to flange thickness ratio	: 0.020	: 0.040	: 0.080	: 0.120	: 0.240	: 0.500
Membrane correction factor Mm	: 1.100	: 1.100	: 1.100	: 1.100	: 1.120	: 1.230
Bending correction factor Mb	: 1.010	: 0.980	: 0.920	: 0.860	: 0.720	: 0.420
Faulted condition (2)						
(a) = Sm*Mm*SQRT(PI)*SQRT(a/Q)	: 5070.570	: 7170.868	: 10141.139	: 12420.308	: 17884.332	: 28349.094
(b) = Sb*Mb*SQRT(PI)*SQRT(a/Q)	: 750.920	: 1030.418	: 1368.013	: 1566.197	: 1854.366	: 1561.319
K1 = (a) + (b)	: 5821.490	: 8201.286	: 11509.152	: 13986.506	: 19738.698	: 29910.413
Faulted condition (3)						
(a) = Sm*Mm*SQRT(PI)*SQRT(a/Q)	: 7975.957	: 11279.706	: 15951.913	: 19537.024	: 28131.879	: 44592.869
(b) = Sb*Mb*SQRT(PI)*SQRT(a/Q)	: 1181.190	: 1620.838	: 2151.871	: 2463.613	: 2916.900	: 2455.940
K1 = (a) + (b)	: 9157.147	: 12900.544	: 18103.784	: 22000.637	: 31048.779	: 47048.788

T45C

Table A 3.10

TROJAN : SUPPORT BEAM CRITICAL FLAW SIZE ANALYSIS

Stress location :	!Mode 4: Max. E4: Inner concrete effective					
Flaw type	!Elliptical surface flaw					
Flaw aspect ratio a/l	! 0.500					
Plate thickness in.	! 2.500					
Stresses in beam flange without flaw :	!Unit load	!Load case	!Load case	!Max. surface	!Flange mean	!Flange bend. :
	!(200 Kip) BM	!maximum load	!maximum BM	!stress	!stress	!stress
Faulted (2) = DW + T + SB:LOCA	! 1333000.000	! 1538000.000	! 10384070.000	! 17031.131	! 14665.696	! 2365.435
Faulted (3) = DW + T + LB:LOCA	! 1333000.000	! 2443000.000	! 16282595.000	! 26705.426	! 22996.339	! 3709.087
Flaw shape parameter = Q	Sy =	40000.000	!Faulted(2)	(Sm + Sb)/Sy =	0.426	Q = 2.370
			!Faulted(3)	(Sm + Sb)/Sy =	0.668	Q = 2.370
Flaw depth "a" in.	0.050	0.100	0.200	0.300	0.600	1.250
Flaw depth to flange thickness ratio	0.020	0.040	0.080	0.120	0.240	0.500
Membrane correction factor Mm	1.100	1.100	1.100	1.100	1.100	1.100
Bending correction factor Mb	0.990	0.960	0.900	0.840	0.670	0.300
Faulted condition (2)						
(a) = Sm*Mm*2SQRT(PI)*2SQRT(a/Q)	4153.184	5873.489	8306.367	10173.181	14387.050	20765.919
(b) = Sb*Mb*2SQRT(PI)*2SQRT(a/Q)	602.882	826.767	1096.148	1253.002	1413.391	913.457
KI = (a) + (b)	4756.065	6700.255	9402.516	11426.183	15800.441	21679.375
Faulted condition (3)						
(a) = Sm*Mm*2SQRT(PI)*2SQRT(a/Q)	6512.341	9209.841	13024.683	15951.913	22559.412	32561.707
(b) = Sb*Mb*2SQRT(PI)*2SQRT(a/Q)	945.340	1296.400	1718.800	1964.752	2216.247	1432.333
KI = (a) + (b)	7457.681	10506.241	14743.482	17916.665	24775.659	33994.040

T408S

Table A 3.11

TROJAN : SUPPORT BEAM CRITICAL FLAW SIZE ANALYSIS

Stress location :	Node 4: Max. BM: Inner concrete not effective					
Flaw type	Uniform depth surface flaw					
Flaw aspect ratio a/l	0.000					
Plate thickness in.	2.500					
Stresses in beam flange without flaw :	Unit load	Load case	Load case	Max. surface	Flange mean	Flange bend.
	(200 Kip) BM	maximum load	maximum BM	stress	stress	stress
Normal + Upset = DW + T + OBE	1527000.000	1099000.000	8390865.000	13762.034	11850.640	1911.394
Faulted (1) = DW + T + SSE	1527000.000	1212000.000	9253620.000	15177.057	13069.132	2107.925
Flaw shape parameter = Q	Sy =	40000.000	Normal+Upset	(Sm + Sb)/Sy =	0.344	Q = 1.000
			Faulted(1)	(Sm + Sb)/Sy =	0.379	Q = 0.980
Flaw depth "a" in.	0.050	0.100	0.200	0.300	0.600	1.250
Flaw depth to flange thickness ratio	0.020	0.040	0.080	0.120	0.240	0.500
Membrane correction factor Mm	1.130	1.130	1.170	1.230	1.500	3.500
Bending correction factor Mb	1.090	1.060	1.030	1.020	1.060	1.580
Normal plus upset conditions						
(a) = $S_m * M_m * \sqrt{PI} * \sqrt{a/Q}$	5307.380	7505.769	10990.504	14150.849	24405.271	82193.941
(b) = $S_b * M_b * \sqrt{PI} * \sqrt{a/Q}$	825.727	1135.615	1560.549	1892.718	2781.676	5984.628
K1 = (a) + (b)	6133.107	8641.384	12551.053	16043.567	27186.947	88178.569
Faulted condition (1)						
(a) = $S_m * M_m * \sqrt{PI} * \sqrt{a/Q}$	5912.513	8361.556	12243.610	15764.289	27187.891	91565.463
(b) = $S_b * M_b * \sqrt{PI} * \sqrt{a/Q}$	919.874	1265.094	1738.478	2109.520	3098.835	6666.978
K1 = (a) + (b)	6832.387	9626.650	13982.088	17872.809	30286.726	98232.441

TROJAN : SUPPORT BEAM CRITICAL FLAW SIZE ANALYSIS

Stress location :	:Node 4: Max. BM: Inner concrete not effective					
Flaw type	:Elliptical surface flaw					
Flaw aspect ratio a/l	: 0.100					
Plate thickness in.	: 2.500					
Stresses in beam flange without flaw :	:Unit load	:Load case	:Load case	:Max. surface	:Flange mean	:Flange bend. :
	::(200 Kip) BM	:maximum load	:maximum BM	:stress	:stress	:stress
	:	:	:	:	:	:
Normal + Upset = DW + T + OBE	: 1527000.000	: 1099000.000	: 8390865.000	: 13762.034	: 11850.640	: 1911.394
Faulted (1) = DW + T + SSE	: 1527000.000	: 1212000.000	: 9253620.000	: 15177.057	: 13069.132	: 2107.925
Flaw shape parameter = Q	: Sy =	40000.000	:Normal+Upset	(Sm + Sb)/Sy =	0.344	: Q = 1.100
	:		:Faulted(1)	(Sm + Sb)/Sy =	0.379	: Q = 1.070
Flaw depth "a" in.	: 0.050	: 0.100	: 0.200	: 0.300	: 0.600	: 1.250
Flaw depth to flange thickness ratio	: 0.020	: 0.040	: 0.080	: 0.120	: 0.240	: 0.500
Membrane correction factor Mm	: 1.100	: 1.100	: 1.120	: 1.140	: 1.250	: 1.810
Bending correction factor Mb	: 1.040	: 1.020	: 0.980	: 0.940	: 0.870	: 0.890
Normal plus upset conditions						
(a) = Sm*Mm*2SQRT(PI)*2SQRT(a/Q)	: 4926.042	: 6966.475	: 10031.213	: 12505.063	: 19391.261	: 40527.890
(b) = Sb*Mb*2SQRT(PI)*2SQRT(a/Q)	: 751.185	: 1041.907	: 1415.695	: 1663.096	: 2176.825	: 3214.206
K1 = (a) + (b)	: 5677.227	: 8008.382	: 11446.908	: 14168.159	: 21568.086	: 43742.096
Faulted condition (1)						
(a) = Sm*Mm*2SQRT(PI)*2SQRT(a/Q)	: 5508.172	: 7789.731	: 11216.641	: 13982.836	: 21682.803	: 45317.232
(b) = Sb*Mb*2SQRT(PI)*2SQRT(a/Q)	: 839.956	: 1165.033	: 1582.994	: 1859.630	: 2434.070	: 3594.042
K1 = (a) + (b)	: 6348.128	: 8954.764	: 12799.634	: 15842.466	: 24116.873	: 48911.274

T42BS

Table A 3.13

TROJAN : SUPPORT BEAM CRITICAL FLAW SIZE ANALYSIS

Stress location :	!Node 41: Max. E4: Inner concrete not effective					
Flaw type	!Elliptical surface flaw					
Flaw aspect ratio a/l	! 0.200					
Plate thickness in.	! 2.500					
	!					
Stresses in beam flange without flaw :	!Unit load	!Load case	!Load case	!Max. surface	!Flange mean	!Flange bend. :
	!(200 Kip) BM	!maximum load	!maximum BM	!stress	!stress	!stress :
	!	!	!	!	!	!
Normal + Upset = DW + T + OBE	! 1527000.000	! 1999000.000	! 8390865.000	! 13762.034	! 11850.640	! 1911.394 :
Faulted (1) = DW + T + SSE	! 1527000.000	! 1212000.000	! 9253620.000	! 15177.057	! 13069.132	! 2107.925 :
	!					
Flaw shape parameter = Q	! Sy =	! 40000.000	!Normal+Upset	! (Sa + Sb)/Sy =	! 0.344	! Q = 1.320
	!		!Faulted(1)	! (Sa + Sb)/Sy =	! 0.379	! Q = 1.270
	!					
Flaw depth "a" in.	! 0.050	! 0.100	! 0.200	! 0.300	! 0.600	! 1.250 :
Flaw depth to flange thickness ratio	! 0.020	! 0.040	! 0.080	! 0.120	! 0.240	! 0.500 :
Membrane correction factor Ma	! 1.100	! 1.100	! 1.100	! 1.100	! 1.150	! 1.400 :
Bending correction factor Mb	! 1.030	! 1.000	! 0.950	! 0.890	! 0.770	! 0.570 :
	!					
Normal plus upset conditions	!					
	!					
(a) = $S_m \cdot M_m \cdot 2\sqrt{PI} \cdot 2\sqrt{a/Q}$	! 4496.840	! 6359.493	! 8993.681	! 11014.964	! 16285.581	! 28616.257 :
(b) = $S_b \cdot M_b \cdot 2\sqrt{PI} \cdot 2\sqrt{a/Q}$	! 679.142	! 932.477	! 1252.785	! 1437.437	! 1758.751	! 1879.178 :
K1 = (a) + (b)	! 5175.982	! 7291.970	! 10246.466	! 12452.401	! 18044.332	! 30495.435 :
	!					
Faulted condition (1)	!					
	!					
(a) = $S_m \cdot M_m \cdot 2\sqrt{PI} \cdot 2\sqrt{a/Q}$	! 5055.889	! 7150.106	! 10111.777	! 12384.348	! 18310.208	! 32173.837 :
(b) = $S_b \cdot M_b \cdot 2\sqrt{PI} \cdot 2\sqrt{a/Q}$	! 763.573	! 1048.403	! 1408.532	! 1616.139	! 1977.400	! 2112.798 :
K1 = (a) + (b)	! 5819.461	! 8198.509	! 11520.310	! 14000.487	! 20287.608	! 34286.635 :

T4385

Table 3.14

TROJAN : SUPPORT BEAM CRITICAL FLAW SIZE ANALYSIS

Stress location :	!Mode 4: Max. BM: Inner concrete not effective					
Flaw type	!Elliptical surface flaw					
Flaw aspect ratio a/l	: 0.300					
Plate thickness in.	: 2.500					
Stresses in beam flange without flaw :	!Unit load	!Load case	!Load case	!Max. surface	!Flange mean	!Flange bend. :
	!(200 Kip) BM	!maximum load	!maximum BM	!stress	!stress	!stress
Normal + Upset = DW + T + OBE	: 1527000.000	: 1099000.000	: 8390865.000	: 13762.034	: 11850.640	: 1911.394
Faulted (I) = DW + T + SSE	: 1527000.000	: 1212000.000	: 9253620.000	: 15177.057	: 13069.132	: 2107.925
Flaw shape parameter = Q	Sy =	40000.000	!Normal+Upset	(Sa + Sb)/Sy =	0.344	Q = 1.650
			!Faulted(I)	(Sa + Sb)/Sy =	0.379	Q = 1.620
Flaw depth "a" in.	: 0.050	: 0.100	: 0.200	: 0.300	: 0.600	: 1.250
Flaw depth to flange thickness ratio	: 0.020	: 0.040	: 0.080	: 0.120	: 0.240	: 0.500
Membrane correction factor Ma	: 1.100	: 1.100	: 1.100	: 1.100	: 1.120	: 1.230
Bending correction factor Mb	: 1.010	: 0.980	: 0.920	: 0.860	: 0.720	: 0.420
Normal plus upset conditions						
(a) = $S_m M_m \sqrt{PI} \sqrt{a/Q}$	: 4022.096	: 5688.103	: 8044.193	: 9852.084	: 14186.277	: 22487.175
(b) = $S_b M_b \sqrt{PI} \sqrt{a/Q}$	: 595.648	: 817.352	: 1085.140	: 1242.345	: 1470.927	: 1238.475
KI = (a) + (b)	: 4617.744	: 6505.455	: 9129.333	: 11094.429	: 15657.204	: 23725.650
Faulted condition (I)						
(a) = $S_m M_m \sqrt{PI} \sqrt{a/Q}$	: 4476.534	: 6330.775	: 8953.067	: 10965.223	: 15799.116	: 25027.893
(b) = $S_b M_b \sqrt{PI} \sqrt{a/Q}$	: 662.947	: 909.701	: 1207.745	: 1382.711	: 1637.120	: 1378.405
KI = (a) + (b)	: 5139.481	: 7240.475	: 10160.813	: 12347.935	: 17426.237	: 26406.298

TROJAN : SUPPORT BEAM CRITICAL FLAW SIZE ANALYSIS

Stress location :	:Node 4: Max. BM: Inner concrete not effective						
Flaw type	:Elliptical surface flaw						
Flaw aspect ratio a/l	: 0.500						
Plate thickness in.	: 2.500						
Stresses in beam flange without flaw :	:Unit load	:Load case	:Load case	:Max. surface	:Flange mean	:Flange bend.	:
	:(200 Kip) BM	:maximum load	:maximum BM	:stress	:stress	:stress	:
	:	:	:	:	:	:	:
Normal + Upset = DW + T + OBE	: 1527000.000	: 1099000.000	: 8300865.000	: 13762.034	: 11850.640	: 1911.394	:
Faulted (1) = DW + T + SSE	: 1527000.000	: 1212000.000	: 9253620.000	: 15177.057	: 13069.132	: 2107.925	:
Flaw shape parameter = Q	Sy =	40000.000	:Normal+Upset	(Sa + Sb)/Sy =	0.344	Q =	2.400
			:Faulted(1)	(Sa + Sb)/Sy =	0.379	Q =	2.380
Flaw depth "a" in.	: 0.050	: 0.100	: 0.200	: 0.300	: 0.600	: 1.250	:
Flaw depth to flange thickness ratio	: 0.020	: 0.040	: 0.080	: 0.120	: 0.240	: 0.500	:
Membrane correction factor Mm	: 1.100	: 1.100	: 1.100	: 1.100	: 1.100	: 1.100	:
Bending correction factor Mb	: 0.990	: 0.960	: 0.900	: 0.840	: 0.670	: 0.300	:
Normal plus upset conditions							
(a) = $S_m \cdot M_m \cdot 2\sqrt{\pi} \cdot \sqrt{a/Q}$	: 3334.946	: 4716.326	: 6669.892	: 8168.916	: 11552.592	: 16674.731	:
(b) = $S_b \cdot M_b \cdot 2\sqrt{\pi} \cdot \sqrt{a/Q}$	: 484.105	: 663.882	: 880.191	: 1006.142	: 1134.932	: 733.493	:
KI = (a) + (b)	: 3819.051	: 5380.208	: 7550.083	: 9175.058	: 12687.524	: 17408.223	:
Faulted condition (1)							
(a) = $S_m \cdot M_m \cdot 2\sqrt{\pi} \cdot \sqrt{a/Q}$	: 3693.269	: 5223.070	: 7386.537	: 9046.623	: 12793.858	: 18466.343	:
(b) = $S_b \cdot M_b \cdot 2\sqrt{\pi} \cdot \sqrt{a/Q}$	: 536.120	: 735.212	: 974.763	: 1114.247	: 1256.875	: 812.302	:
KI = (a) + (b)	: 4229.388	: 5958.283	: 8361.300	: 10160.870	: 14050.732	: 19278.645	:

T40CS

Table A 3.16

TROJAN : SUPPORT BEAM CRITICAL FLAW SIZE ANALYSIS

Stress location :	!Node 4: Max. E%: Inner concrete effective						
Flaw type	!Uniform depth surface flaw						
Flaw aspect ratio a/l	: 0.000						
Plate thickness in.	: 2.500						
Stresses in beam flange without flaw :	!Unit load	!Load case	!Load case	!Max. surface	!Flange mean	!Flange bend.	!
	!(200 Kip) BM	!maximum load	!maximum BM	!stress	!stress	!stress	!
Normal + Upset = DW + T + OBE	: 1333000.000	: 1099000.000	: 7324835.000	: 12013.616	: 10343.058	: 1668.558	!
Faulted (I) = DW + T + SSE	: 1333000.000	: 1212000.000	: 8077980.000	: 13248.865	: 11408.745	: 1840.120	!
Flaw shape parameter = Q	Sy =	40000.000	!Normal+Upset	(Sm + Sb)/Sy =	0.300	Q =	1.010
			!Faulted(I)	(Sm + Sb)/Sy =	0.331	Q =	0.990
Flaw depth "a" in.	: 0.050	: 0.100	: 0.200	: 0.300	: 0.600	: 1.250	!
Flaw depth to flange thickness ratio	: 0.020	: 0.040	: 0.080	: 0.120	: 0.240	: 0.500	!
Membrane correction factor Me	: 1.130	: 1.130	: 1.170	: 1.230	: 1.500	: 3.500	!
Bending correction factor Mb	: 1.090	: 1.060	: 1.030	: 1.020	: 1.060	: 1.580	!
Normal plus upset conditions							
(a) = $S_m * M_m * \sqrt{PI} * \sqrt{a/Q}$	: 4610.103	: 6519.670	: 9546.585	: 12291.727	: 21198.936	: 71395.400	!
(b) = $S_b * M_b * \sqrt{PI} * \sqrt{a/Q}$	: 717.244	: 986.419	: 1355.526	: 1644.055	: 2416.223	: 5198.375	!
KI = (a) + (b)	: 5327.347	: 7506.089	: 10902.110	: 13935.782	: 23615.159	: 76593.775	!
Faulted condition (I)							
(a) = $S_m * M_m * \sqrt{PI} * \sqrt{a/Q}$	: 5135.215	: 7262.291	: 10633.985	: 13691.812	: 23613.593	: 79527.668	!
(b) = $S_b * M_b * \sqrt{PI} * \sqrt{a/Q}$	: 798.942	: 1098.777	: 1509.926	: 1831.320	: 2691.442	: 5790.494	!
KI = (a) + (b)	: 5934.157	: 8361.067	: 12143.912	: 15523.133	: 26305.035	: 85318.162	!

T4ICS

Table A 3.17

TROJAN : SUPPORT BEAM CRITICAL FLAW SIZE ANALYSIS

Stress location :	:Node 4: Max. BM: Inner concrete effective					
Flaw type	:Elliptical surface flaw					
Flaw aspect ratio a/l	: 0.100					
Plate thickness in.	: 2.500					
Stresses in beam flange without flaw :	:Unit load	:Load case	:Load case	:Max. surface	:Flange mean	:Flange bend. :
	:(200 Kip) BM	:maximum load	:maximum BM	:stress	:stress	:stress :
	:	:	:	:	:	:
Normal + Upset = DW + T + DBE	: 1333000.000	: 1099000.000	: 7324835.000	: 12013.616	: 10345.058	: 1668.558 :
Faulted (I) = DW + T + SSE	: 1333000.000	: 1212000.000	: 8077980.000	: 13248.865	: 11408.745	: 1840.120 :
	:	:	:	:	:	:
Flaw shape parameter = Q	Sy =	40000.000	:Normal+Upset	(Sa + Sb)/Sy =	0.300	Q = 1.110
			:Faulted(I)	(Sa + Sb)/Sy =	0.331	Q = 1.090
	:	:	:	:	:	:
Flaw depth "a" in.	: 0.050	: 0.100	: 0.200	: 0.300	: 0.600	: 1.250 :
Flaw depth to flange thickness ratio	: 0.020	: 0.040	: 0.080	: 0.120	: 0.240	: 0.500 :
Membrane correction factor Ma	: 1.100	: 1.100	: 1.120	: 1.140	: 1.250	: 1.810 :
Bending correction factor Mb	: 1.040	: 1.020	: 0.980	: 0.940	: 0.870	: 0.890 :
	:	:	:	:	:	:
Normal plus upset conditions	:	:	:	:	:	:
	:	:	:	:	:	:
(a) = $S_m \cdot M_m \cdot 2\sqrt{\pi} \cdot \sqrt{a/Q}$	: 4280.791	: 6053.953	: 8717.248	: 10867.055	: 16851.246	: 35219.238 :
(b) = $S_b \cdot M_b \cdot 2\sqrt{\pi} \cdot \sqrt{a/Q}$	: 652.789	: 905.430	: 1230.257	: 1445.251	: 1891.688	: 2793.185 :
KI = (a) + (b)	: 4933.581	: 6959.383	: 9947.505	: 12312.305	: 18742.934	: 38012.423 :
	:	:	:	:	:	:
Faulted condition (I)	:	:	:	:	:	:
	:	:	:	:	:	:
(a) = $S_m \cdot M_m \cdot 2\sqrt{\pi} \cdot \sqrt{a/Q}$	: 4764.060	: 6737.398	: 9701.359	: 12093.862	: 18753.623	: 39195.222 :
(b) = $S_b \cdot M_b \cdot 2\sqrt{\pi} \cdot \sqrt{a/Q}$	: 726.484	: 1007.646	: 1369.143	: 1608.408	: 2105.245	: 3108.514 :
KI = (a) + (b)	: 5490.544	: 7745.045	: 11070.502	: 13702.271	: 20858.868	: 42303.736 :

T42CS

Table A 3.18

TROJAN : SUPPORT BEAM CRITICAL FLAW SIZE ANALYSIS

Stress location :	:Node 41: Max. BM: Inner concrete effective					
Flaw type	:Elliptical surface flaw					
Flaw aspect ratio a/l	: 0.200					
Plate thickness in.	: 2.500					
Stresses in beam flange without flaw :	:Unit load	:Load case	:Load case	:Max. surface	:Flange mean	:Flange bend. :
	:(200 Kip) BM	:maximum load	:maximum BM	:stress	:stress	:stress
Normal + Upset = DW + T + OBE	: 1333000.000	: 1099000.000	: 7324835.000	: 12013.616	: 10345.058	: 1668.558
Faulted (I) = DW + T + SSE	: 1333000.000	: 1212000.000	: 8077980.000	: 13248.865	: 11408.745	: 1840.120
Flaw shape parameter = Q	: Sy =	40000.000	:Normal+Upset	(S _m + S _b)/Sy =	0.300	: Q = 1.330
			:Faulted(I)	(S _m + S _b)/Sy =	0.331	: Q = 1.300
Flaw depth "a" in.	: 0.050	: 0.100	: 0.200	: 0.300	: 0.600	: 1.250
Flaw depth to flange thickness ratio	: 0.020	: 0.040	: 0.080	: 0.120	: 0.240	: 0.500
Membrane correction factor M _m	: 1.100	: 1.100	: 1.100	: 1.100	: 1.150	: 1.400
Bending correction factor M _b	: 1.030	: 1.000	: 0.950	: 0.890	: 0.770	: 0.570
Normal plus upset conditions						
(a) = S _m *M _m *SQRT(PI)*SQRT(a/Q)	: 3910.747	: 5530.632	: 7821.494	: 9579.335	: 14163.008	: 24886.573
(b) = S _b *M _b *SQRT(PI)*SQRT(a/Q)	: 590.626	: 810.943	: 1089.504	: 1250.089	: 1529.525	: 1634.256
K _I = (a) + (b)	: 4501.373	: 6341.575	: 8910.999	: 10829.424	: 15692.534	: 26520.829
Faulted condition (I)						
(a) = S _m *M _m *SQRT(PI)*SQRT(a/Q)	: 4362.333	: 6169.270	: 8724.666	: 10685.490	: 15798.454	: 27760.300
(b) = S _b *M _b *SQRT(PI)*SQRT(a/Q)	: 658.927	: 904.585	: 1215.313	: 1394.441	: 1706.144	: 1822.969
K _I = (a) + (b)	: 5021.160	: 7073.855	: 9939.978	: 12079.930	: 17504.598	: 29583.269

T43CS

Table A 3.19

TROJAN : SUPPORT BEAM CRITICAL FLAW SIZE ANALYSIS

Stress location :	:Node 4: Max. BM: Inner concrete effective					
Flaw type	:Elliptical surface flaw					
Flaw aspect ratio a/l	: 0.300					
Plate thickness in.	: 2.500					
Stresses in beam flange without flaw :	:Unit load	:Load case	:Load case	:Max. surface	:Flange mean	:Flange bend. :
	: (200 Kip) BM	: maximum load	: maximum BM	: stress	: stress	: stress
Normal + Upset = DW + T + OBE	: 1333000.000	: 1099000.000	: 7324835.000	: 12013.616	: 10345.058	: 1668.558
Faulted (1) = DW + T + SSE	: 1333000.000	: 1212000.000	: 8077980.000	: 13248.865	: 11408.745	: 1840.120
Flaw shape parameter = Q	: Sy =	40000.000	:Normal+Upset	(Sm + Sb)/Sy =	0.300	: Q = 1.670
			:Faulted(1)	(Sm + Sb)/Sy =	0.331	: Q = 1.630
Flaw depth "a" in.	: 0.050	: 0.100	: 0.200	: 0.300	: 0.600	: 1.250
Flaw depth to flange thickness ratio	: 0.020	: 0.040	: 0.080	: 0.120	: 0.240	: 0.500
Membrane correction factor Mm	: 1.100	: 1.100	: 1.100	: 1.100	: 1.120	: 1.230
Bending correction factor Mb	: 1.010	: 0.980	: 0.920	: 0.860	: 0.720	: 0.420
Normal plus upset conditions						
(a) = Sm*Mm*SQRT(PI)*SQRT(a/Q)	: 3490.015	: 4935.627	: 6980.030	: 8548.756	: 12309.581	: 19512.358
(b) = Sb*Mb*SQRT(PI)*SQRT(a/Q)	: 516.850	: 709.225	: 941.588	: 1077.996	: 1276.339	: 1074.638
K1 = (a) + (b)	: 4006.865	: 5644.852	: 7921.618	: 9626.752	: 13585.920	: 20586.996
Faulted condition (1)						
(a) = Sm*Mm*SQRT(PI)*SQRT(a/Q)	: 3895.800	: 5509.494	: 7791.600	: 9542.723	: 13740.820	: 21781.065
(b) = Sb*Mb*SQRT(PI)*SQRT(a/Q)	: 576.944	: 791.687	: 1051.066	: 1203.335	: 1424.739	: 1199.587
K1 = (a) + (b)	: 4472.744	: 6301.180	: 8842.667	: 10746.057	: 15165.559	: 22980.651

T45CS

Table A 3.20

TROJAN : SUPPORT BEAM CRITICAL FLAW SIZE ANALYSIS

Stress location :	:Node 4: Max. BM: Inner concrete effective					
Flaw type	:Elliptical surface flaw					
Flaw aspect ratio a/l	: 0.500					
Plate thickness in.	: 2.500					
Stresses in beam flange without flaw :	:Unit load	:Load case	:Load case	:Max. surface	:Flange mean	:Flange bend. :
	:(200 Kip) BM	:maximum load	:maximum BM	:stress	:stress	:stress :
	:	:	:	:	:	:
Normal + Upset = DW + T + DBE	: 1333000.000	: 1099000.000	: 7324835.000	: 12013.616	: 10345.058	: 1668.558 :
Faulted (I) = DW + T + SSE	: 1333000.000	: 1212000.000	: 8077980.000	: 13248.865	: 11408.745	: 1840.120 :
Flaw shape parameter = Q	: Sy =	40000.000	:Normal+Upset	(Sm + Sb)/Sy =	0.300	: Q = 2.430
			:Faulted(I)	(Sm + Sb)/Sy =	0.331	: Q = 2.400
Flaw depth "a" in.	: 0.050	: 0.100	: 0.200	: 0.300	: 0.600	: 1.250 :
Flaw depth to flange thickness ratio	: 0.020	: 0.040	: 0.080	: 0.120	: 0.240	: 0.500 :
Membrane correction factor Ma	: 1.100	: 1.100	: 1.100	: 1.100	: 1.100	: 1.100 :
Bending correction factor Mb	: 0.990	: 0.960	: 0.900	: 0.840	: 0.670	: 0.300 :
Normal plus upset conditions						
(a) = $S_m M_m + 2\sqrt{PI} \cdot 2\sqrt{a/Q}$	: 2893.226	: 4091.640	: 5786.453	: 7086.928	: 10022.430	: 14466.132 :
(b) = $S_b M_b + 2\sqrt{PI} \cdot 2\sqrt{a/Q}$	: 419.984	: 575.949	: 763.608	: 872.877	: 984.608	: 636.340 :
KI = (a) + (b)	: 3313.211	: 4667.589	: 6550.061	: 7959.805	: 11007.038	: 15102.472 :
Faulted condition (I)						
(a) = $S_m M_m + 2\sqrt{PI} \cdot 2\sqrt{a/Q}$	: 3210.590	: 4540.460	: 6421.180	: 7864.307	: 11121.810	: 16052.950 :
(b) = $S_b M_b + 2\sqrt{PI} \cdot 2\sqrt{a/Q}$	: 466.053	: 639.126	: 847.370	: 968.624	: 1092.612	: 706.142 :
KI = (a) + (b)	: 3676.643	: 5179.586	: 7268.550	: 8832.932	: 12214.422	: 16759.092 :

Table A 3.21

TROJAN: SUPPORT BEAM CRITICAL FLAW SIZE ANALYSIS

STRESS INTENSITY SUMMARYRY : MODE 4 : LOCATION OF MAXIMUM BENDING MOMENT

Load case	Inner Concrete Effective ?	Flaw Aspect Ratio	-----Flaw Depth (in.)-----						
			0.00	0.05	0.10	0.20	0.30	0.60	1.25
DW+T+OBE	No	0.00	0.00	6133.00	3641.00	12551.00	16043.00	27186.00	88178.00
DW+T+OBE	No	0.10	0.00	5677.00	8008.00	11444.00	14168.00	21568.00	43742.00
DW+T+OBE	No	0.20	0.00	5175.00	7291.00	10246.00	12452.00	18044.00	30495.00
DW+T+OBE	No	0.30	0.00	4617.00	6505.00	9129.00	11094.00	15657.00	23725.00
DW+T+OBE	No	0.50	0.00	3819.00	5380.00	7550.00	9175.00	12687.00	17408.00
DW+T+OBE	Yes	0.00	0.00	5327.00	7506.00	10902.00	13935.00	23615.00	76593.00
DW+T+OBE	Yes	0.10	0.00	4933.00	6959.00	9947.00	12312.00	18742.00	38012.00
DW+T+OBE	Yes	0.20	0.00	4501.00	6341.00	8910.00	10829.00	15692.00	26520.00
DW+T+OBE	Yes	0.30	0.00	4006.00	5644.00	7921.00	9626.00	13535.00	20535.00
DW+T+OBE	Yes	0.50	0.00	3313.00	4667.00	6550.00	7959.00	11007.00	15102.00
DW+T+SSE	No	0.00	0.00	6832.00	9626.00	13982.00	17872.00	30286.00	98232.00
DW+T+SSE	No	0.10	0.00	6348.00	8954.00	12799.00	15842.00	24116.00	48911.00
DW+T+SSE	No	0.20	0.00	5819.00	8198.00	11520.00	14000.00	20287.00	34286.00
DW+T+SSE	No	0.30	0.00	5139.00	7240.00	10160.00	12347.00	17426.00	26406.00
DW+T+SSE	No	0.50	0.00	4229.00	5958.00	8361.00	10160.00	14050.00	19278.00
DW+T+SSE	Yes	0.00	0.00	5934.00	8361.00	12143.00	15523.00	26305.00	85318.00
DW+T+SSE	Yes	0.10	0.00	5490.00	7745.00	11070.00	13702.00	20858.00	42303.00
DW+T+SSE	Yes	0.20	0.00	5021.00	7073.00	9939.00	12079.00	17504.00	29583.00
DW+T+SSE	Yes	0.30	0.00	4472.00	6301.00	8842.00	10746.00	15165.00	22980.00
DW+T+SSE	Yes	0.50	0.00	3676.00	5179.00	7268.00	8832.00	12214.00	16759.00
DW+T+SB:LOCA	No	0.00	0.00	9064.76	12772.01	18550.52	23712.47	40182.44	130328.35
DW+T+SB:LOCA	No	0.10	0.00	8399.27	11848.14	16935.33	20961.33	31909.28	64715.01
DW+T+SB:LOCA	No	0.20	0.00	7601.46	10709.00	15047.98	18287.62	26499.95	44785.66
DW+T+SB:LOCA	No	0.30	0.00	6732.55	9484.78	13310.32	16175.38	22827.78	34591.35
DW+T+SB:LOCA	No	0.50	0.00	5483.06	7724.43	10837.75	13172.75	18215.64	24993.20
DW+T+SB:LOCA	Yes	0.00	0.00	7787.17	10971.91	15936.00	20370.42	34519.10	111959.80
DW+T+SB:LOCA	Yes	0.10	0.00	7191.16	10143.96	14499.42	17946.34	27319.59	55406.68
DW+T+SB:LOCA	Yes	0.20	0.00	6530.38	9200.06	12927.67	15710.83	22766.00	38475.19
DW+T+SB:LOCA	Yes	0.30	0.00	5821.49	8201.29	11509.15	13986.51	19738.70	29910.41
DW+T+SB:LOCA	Yes	0.50	0.00	4756.07	6700.26	9402.52	11426.18	15800.44	21679.38
DW+T+LB:LOCA	No	0.00	0.00	14616.00	20594.00	29912.00	38235.00	64792.00	210149.00
DW+T+LB:LOCA	No	0.10	0.00	13439.00	18957.00	27097.00	33538.00	51056.00	103546.00
DW+T+LB:LOCA	No	0.20	0.00	12067.00	17000.00	23888.00	29031.00	42069.00	71097.00
DW+T+LB:LOCA	No	0.30	0.00	10694.00	15066.00	21143.00	25695.00	36262.00	54949.00
DW+T+LB:LOCA	No	0.50	0.00	8653.00	12190.00	17107.00	20789.00	28747.00	39443.00
DW+T+LB:LOCA	Yes	0.00	0.00	12476.00	17578.00	25531.00	32636.00	55304.00	179373.00
DW+T+LB:LOCA	Yes	0.10	0.00	11384.00	16059.00	22955.00	28412.00	43252.00	87719.00
DW+T+LB:LOCA	Yes	0.20	0.00	10363.00	14599.00	20514.00	24931.00	36127.00	61055.00
DW+T+LB:LOCA	Yes	0.30	0.00	9157.00	12900.00	18103.00	22000.00	31048.00	47048.00
DW+T+LB:LOCA	Yes	0.50	0.00	7457.00	10506.00	14743.00	17916.00	24775.00	33994.00

DW+T+OBE = 1099 Kips.

DW+T+SSE = 1212 Kips

DW+T+SB:LOCA= 1558 Kips.

DW+T+LB:LOCA= 2443 Kips.

Appendix 4

TROJAN CRITICAL-FLAW-SIZE ANALYSIS FOR THE
BEAM-TOP FLANGE AT THE LOCATION OF THE
4-in.-diam GROUT HOLES

Table A 4.1

TROJAN : SUPPORT BEAM CRITICAL FLAW SIZE ANALYSIS

Stress location :	4 in. diameter hole in the upper beam flange; inner concrete not effective									
Flaw type	Uniform depth radial flaw									
Hole diameter in.	4.000									
Plate width in.	16.000									
Stresses in beam flange without flaw :	Unit load (200 Kip) BM	Load case maximum load	Load case maximum BM	Max. surface stress	Flange mean stress:Note 1	Flange bend. stress				
Faulted (2) = DW + T + SB:LOCA	1493000.000	1558000.000	11630470.000	19075.378	16426.020	2649.358				
Faulted (3) = DW + T + LB:LOCA	1493000.000	2443000.000	18236995.000	29910.879	25756.590	4154.289				
Flaw depth "a" in.	0.050	0.100	0.150	0.200	0.300	0.400	0.600	1.000	2.000	3.000
Flaw depth to flange width ratio (R+a)/W	0.256	0.263	0.269	0.275	0.288	0.300	0.325	0.375	0.500	0.625
Membrane correction factor = F (Ref. 24)	0.520	0.730	0.840	0.910	1.010	1.065	1.150	1.200	1.300	1.450
Faulted condition (2)										
$K1 = S_m F \sqrt{P1} \sqrt{R+a}$	21676.420	30799.225	35859.630	39297.056	44595.658	48035.522	53987.324	60513.066	75697.343	94397.456
Faulted condition (3)										
$K1 = S_m F \sqrt{P1} \sqrt{R+a}$	33989.406	48294.292	56229.188	61619.197	69927.595	75321.425	84654.064	94886.663	118696.154	148018.604
Notes :	(1) This stress is based on the full flange width in order to match the uniform far field stress boundary condition incorporated into the stress intensity factor equation. (The flange hole has not been taken out)									

Table A 4.2

TROJAN : SUPPORT BEAM CRITICAL FLAW SIZE ANALYSIS

Stress location :	4 in. diameter hole in the upper beam flange: inner concrete effective										
Flaw type	Uniform depth radial flaw										
Hole diameter in.	4.000										
Plate width in.	16.000										
Stresses in beam flange without flaw :	Unit load (200 Kip) BM	Load case maximum load	Load case maximum BM	Max. surface stress	Flange mean stress:Note 1	Flange bend. stress					
Faulted (2) = DW + T + SB:LOCA	1238000.000	1558000.000	9644020.000	15817.360	13620.504	2196.856					
Faulted (3) = DW + T + LB:LOCA	1238000.000	2443000.000	15122170.000	24802.189	21357.440	3444.748					
Flaw depth "a" in.	0.050	0.100	0.150	0.200	0.300	0.400	0.600	1.000	2.000	3.000	
Flaw depth to flange width ratio (R+a)/W	0.256	0.263	0.269	0.275	0.288	0.300	0.325	0.375	0.500	0.625	
Membrane correction factor = F (Ref. 24)	0.520	0.730	0.840	0.910	1.010	1.065	1.150	1.200	1.300	1.450	
Faulted condition (2)											
$K_I = S_m \cdot F \cdot \sqrt{P_I} \cdot \sqrt{R+a}$	17974.151	25538.808	29734.911	32585.235	36978.851	39831.196	44766.448	50177.613	62768.460	78274.649	
Faulted condition (3)											
$K_I = S_m \cdot F \cdot \sqrt{P_I} \cdot \sqrt{R+a}$	28184.116	40045.770	46625.409	51094.819	57984.168	62456.748	70195.400	78680.300	98423.201	122737.462	
Notes :	(1) This stress is based on the full flange width in order to match the uniform far field stress boundary condition incorporated into the stress intensity factor equation. (The flange hole has not been taken out)										

Table A 4.3

TROJAN : SUPPORT BEAM CRITICAL FLAW SIZE ANALYSIS

Stress location :	4 in. diameter hole in the upper beam flange: inner concrete not effective									
Flaw type	Uniform depth radial flaw									
Hole diameter in.	4.000									
Plate width in.	16.000									
Stresses in beam flange without flaw :	Unit load (200 Kip) BM	Load case maximum load	Load case maximum BM	Max. surface stress	Flange mean stress:Note 1	Flange bend. stress				
Normal + Upset = DW + T + OBE	1493000.000	1099000.000	8204035.000	13455.610	11586.775	1868.835				
Faulted (1) = DW + T + SSE	1493000.000	1212000.000	9047580.000	14839.126	12778.136	2060.990				
Flaw depth "a" in.	0.050	0.100	0.150	0.200	0.300	0.400	0.600	1.000	2.000	3.000
Flaw depth to flange width ratio (R+a)/W	0.256	0.263	0.269	0.275	0.288	0.300	0.325	0.375	0.500	0.625
Membrane correction factor = F (Ref. 24)	0.520	0.730	0.840	0.910	1.010	1.065	1.150	1.200	1.300	1.450
Normal plus upset condition										
$K_I = S_m * F * \sqrt{P_I} * \sqrt{R+a}$	15290.363	21725.513	25295.079	27719.811	31457.399	33883.850	38082.201	42685.404	53396.264	66587.166
Faulted condition (1)										
$K_I = S_m * F * \sqrt{P_I} * \sqrt{R+a}$	16862.530	23959.346	27895.938	30569.982	34691.873	37367.813	41997.841	47074.349	58886.508	73433.708
Notes : (1)	This stress is based on the full flange width in order to match the uniform far field stress boundary condition incorporated into the stress intensity factor equation. (The flange hole has not been taken out)									

Table A 4.4

TROJAN : SUPPORT BEAM CRITICAL FLAW SIZE ANALYSIS

Stress location :	4 in. diameter hole in the upper beam flange: inner concrete effective									
Flaw type	Uniform depth radial flaw									
Hole diameter in.	4.000									
Plate width in.	16.000									
Stresses in beam flange without flaw :	Unit load (200 Kip) BM	Load case maximum load	Load case maximum BM	Max. surface stress	Flange mean stress:Note 1	Flange bend. stress				
Normal + Upset = DW + T + OBE	1238000.000	1099000.000	6802810.000	11157.432	9607.788	1549.643				
Faulted (1) = DW + T + SSE	1238000.000	1212000.000	7502280.000	12304.647	10595.668	1708.979				
Flaw depth "a" in.	0.050	0.100	0.150	0.200	0.300	0.400	0.600	1.000	2.000	3.000
Flaw depth to flange width ratio (R+a)/W	0.256	0.263	0.269	0.275	0.288	0.300	0.325	0.375	0.500	0.625
Membrane correction factor = F (Ref. 24)	0.520	0.730	0.840	0.910	1.010	1.065	1.150	1.200	1.300	1.450
Normal plus upset condition										
$K_I = S_m \cdot F \cdot \sqrt{\pi} \cdot \sqrt{R+a}$	12678.814	18014.859	20974.754	22985.349	26084.568	28096.588	31577.873	35394.863	44276.340	55214.274
Faulted condition (1)										
$K_I = S_m \cdot F \cdot \sqrt{\pi} \cdot \sqrt{R+a}$	13982.459	19867.160	23131.394	25348.719	28766.603	30985.501	34824.734	39034.189	48828.866	60891.447
Notes : (1)	This stress is based on the full flange width in order to match the uniform far field stress boundary condition incorporated into the stress intensity factor equation. (The flange hole has not been taken out)									

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Table A4.5

TROJAN: SUPPORT BEAM CRITICAL FLAW SIZE ANALYSIS

STRESS INTENSITY SUMMARY

4 IN. DIAMETER HOLE IN THE UPPER BEAM FLANGE

Load case	Inner Concrete Effective ?	Flaw Aspect Ratio	Flaw Depth (in.)										
			0.000	0.050	0.100	0.150	0.200	0.300	0.400	0.600	1.000	2.000	3.000
DU+T+OBE	No	0.000	0.000	15290.363	21725.513	25295.079	27719.811	31457.399	33883.850	38082.201	42685.404	53396.264	66587.166
DU+T+OBE	Yes	0.000	0.000	12678.814	18014.859	20974.754	22985.349	26084.568	28096.588	31577.873	35394.863	44276.340	55214.274
DU+T+SSE	No	0.000	0.000	16862.530	23959.346	27895.938	30569.982	34691.873	37367.813	41997.841	47074.349	58886.508	73433.708
DU+T+SSE	Yes	0.000	0.000	13982.459	19867.160	23131.394	25348.719	28766.603	30985.501	34824.734	39034.189	48828.866	60891.447
DU+T+SB:LOCA	No	0.000	0.000	21676.420	30799.225	35859.630	39297.056	44595.658	48035.522	53987.324	60513.066	75697.343	94397.456
DU+T+SB:LOCA	Yes	0.000	0.000	17974.151	25538.808	29734.911	32585.235	36978.851	39831.196	44766.448	50177.613	62768.460	78274.649
DU+T+LB:LOCA	No	0.000	0.000	33989.406	48294.292	56229.188	61619.197	69927.595	75321.425	84654.064	94886.663	118696.154	148018.604
DU+T+LB:LOCA	Yes	0.000	0.000	28184.116	40045.770	46625.409	51094.819	57984.168	62456.748	70195.400	78680.300	98423.201	122737.462

Appendix 5

AN EVALUATION OF THE IMPACT OF ASSUMPTIONS MADE IN MODELING
THE VESSEL-SUPPORT BEAM ELASTIC FOUNDATIONA5.1 Background

Several assumptions were made in modeling the concrete foundation elements in the original analysis. The sensitivity of the beam bending moment diagram to these assumptions will now be assessed. The assumptions of concern are as follows:

1. The concrete compressive modulus was set at 6×10^6 lb/in. in an attempt to reflect the maximum foundation stiffness. Concrete stiffnesses down to $E = 5 \times 10^6$ lb/in.² will be evaluated.
2. Two rings of vertical rebar exist near the inner pedestal and were not included in the foundation stiffness.
3. Two wide-flange struts that join the upper and lower supports for alignment and assembly purposes were not included in the analyses. These will now be added.

In total, this foundation modeling sensitivity assessment is comprised of six cases. A summary of the parameter combination included in each of the cases is given in Fig. A5.1.

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STUDY CASE NUMBER	FOUNDATION PARAMETER			
	EFFECTIVE INNER CONCRETE	REINFORCING STEEL AND VERTICAL BEAM	CONCRETE E_c	
			5×10^6	6×10^6
1				
2				
3				
4				
5				
6				
COMMENTS	INCLUDED IN THE BASE CASE ANALYSIS	ADDITIONAL CASES RUN FOR THE FOUNDATION MODELING SENSITIVITY ANALYSIS		

Fig. A5.1. Study scope.

A5.2 Geometry of reinforcing steel

A section through the cavity wall showing both the distribution of the reinforcing bars and the beam elastic foundation footprint is given in Fig. A5.2. Derivation of the foundation footprint average width (32.75 in.) is given in Sect. 6.4.2.3 of the report. Reinforcing bars falling within the foundation footprint were included in the foundation stiffness on a case-by-case basis, as indicated in Fig. A5.1.

The radius to the beam lug centerline is 9 ft 2-1/2 in. Offsets to the rebar centerlines are

- (1) inner circle: $x = 124 - 110.5 = 13.5$ in.
- (2) outer circle: $x = 140 - 110.5 = 29.5$ in.

In addition to the reinforcing bars, two vertical wide-flange members are incorporated into the vessel-support assembly design. The primary purpose of these vertical members is to join the upper and lower vessel-support assemblies and thereby maintain alignment between the two assemblies during erection. They do, however, provide some additional

Fig. A5.2. Reinforcing bar distribution.

vertical restraint to the beam and were therefore included in the modified foundation model along with the reinforcing steel. The vertical wide-flange members are located 45 in. from the beam loading point and have an area of 8.25 in.² each ($I_{xx} = 98.0$ in.⁴).

A5.3 Analytical model adjustment

The inner rebar circle offset (13.5 in.) aligns closely with the inner flange of the front pedestal. The rebar was therefore added directly to the front-pedestal, front-flange area (see beam No. 22 extending between nodes 3 and 23). The No. 8 rebar is spaced on 18.0-in. centers, giving a maximum of two bars to be added to the flange area; that is,

$$A = 20 + 2 \times 0.79 = 21.58 \text{ in.}^2 .$$

The outer rebar circle offset (29.5) is located midway between nodes 9 and 10. The rebar area was added in its entirety to foundation beam No. 25, which represents the inner pedestal outer flange. The average width of a concrete support element is obtained from Sect. 6.4.2.3 as 32.75 in. The additional steel area for this location is thus $(32.75/12) \times 1.27 = 3.47$ in.².

Details of the model modifications made to incorporate the reinforcing steel into the foundation elements are given in Fig. A5.3. The addition of the vertical wide-flange beam to the model is shown in Fig. A5.4(b).

Fig. A5.3. Reinforcing bar representation.

Fig. A5.4. Analytical model: (a) complete model and (b) detail of vertical beam modeling.

Evaluation of the impact of a reduced elastic modulus for the concrete required that the model foundation member areas be modified in some locations, because areas of mixed steel and concrete foundation were modeled as equivalent concrete members. The adjusted areas, reflecting the effect of the reduced concrete elastic modulus, are shown in Fig. A5.5.

Fig. A5.5. Concrete elastic modulus adjustment.

A5.4 Foundation sensitivity analysis

Separate runs of the finite-element model were made for each of the cases identified in Fig. A5.1. Beam bending moments and foundation reactions obtained from those runs are summarized in Tables A5.1 and A5.2, respectively.

Table A5.1

BEAM FOUNDATION PARAMETER SENSITIVITY ANALYSIS

EFFECT ON BEAM BENDING MOMENTS

PARAMETERS : (1) Inner concrete (IC) : I = Included, NI = Not include

(2) Re-bar and vertical beam (RS) : I = Included

NI = Not included

(3) Concrete elastic modulus (E) : Low modulus = 5×10^6

High modulus = 6×10^6

	Case 1	Case 2	Case 3	Case 4	Case 5	Case 6
Inner concrete	I	NI	I	NI	I	NI
Rebar & beam	NI	NI	I	I	I	I
Concrete E	6×10^6	6×10^6	6×10^6	6×10^6	5×10^6	5×10^6
Distance from loading point	-----Bending Moments-----					
0	0	0	0	0	0	0
11.35	1134999	1134998	1134979	1134999	1134979	1134998
13.63	1262492	1362989	1263739	1362989	1273778	1362989
16.5	1333322	1527073	1370371	1513121	1344810	1517823
19	1280420	1507885	1275598	1494721	1296557	1497669
21	1230098	1492534	1231773	1479944	1257954	1479946
23	1150574	1407460	1149704	1400077	1174735	1394321
25.5	1041168	1301113	1033611	1300243	1070712	1287289
28.37	895404	1138629	887837	1146948	929824	1125302
31.54	734234	952125	726921	948200	772636	939426
36.63	498121	665990	491477	691656	530632	654631
41.72	305460	424588	299223	454118	343113	414470
46.81	162590	240071	153910	267976	193874	231134
51.9	66882	112108	63624	137646	72204	106047
56.99	10833	33555	9246	53410	28283	30784
62.08	-15153	-6653	-15712	6075	-4470	-7768
65.16	-18781	-14972	-19004	-6592	-12076	-15520
67.79	-18408	-17337	-18443	-11594	-14073	-17547
72	-11057	-11421	-11009	-9074	-9469	-11421
76.21	-2581	-2797	-2560	-2553	-2502	-2782
78.84	0	0	0	0	0	0
Max. BM	1333322	1527073	1330371	1513121	1344810	1517823
Min. BM	-18781	-17337	-19004	-11594	-14073	-17547

Table A5.2

BEAM FOUNDATION PARAMETER SENSITIVITY ANALYSIS
EFFECT ON BEAM FOUNDATION REACTIONS

PARAMETERS : (1) Inner concrete (IC) : I = Included. NI = Not include

(2) Re-bar and vertical beam (RS) : I = Included

NI = Not included

(3) Concrete elastic modulus (E) : Low modulus = 5×10^6

High modulus = 6×10^6

	Case 1	Case 2	Case 3	Case 4	Case 5	Case 6
Inner concrete	I	NI	I	NI	I	NI
Rebar & beam	NI	NI	I	I	I	I
Concrete E	6×10^6	6×10^6	6×10^6	6×10^6	5×10^6	5×10^6
Distance from loading point	-----Foundation Reactions-----					
0	0	0	0	0	0	0
11.35	44079	0	43532	0	39129	0
13.63	31241	42827	33250	45353	36121	47664
16.5	45840	64848	45127	63508	44052	59724
21	22602	34861	22128	33951	22308	32545
25.5	7026	14079	6754	13524	7480	13478
28.37	54	2219	-30	2395	479	2967

A5.5 Results

Bending moments generated by unit loading on each of the six models are plotted in Fig. A5.6. The curves fall into two families, corresponding to the two inner concrete support conditions evaluated. Peak bending moments for the family with inner-concrete foundation support included in the model are ~13% lower than those from the family with no inner-concrete support. Within the families, however, changes in the modeling of the reinforcing steel and the stiffness of the concrete influence the peak beam bending moments by <1%.

The effect of the foundation modeling assumptions on the distribution of reaction forces between the foundation members is shown in Fig. A5.7. The effect of the modeling changes is to move load onto the steel elements and away from the concrete elements. Maximum load shifts are <13%. Note that the flaw tolerance of the pedestal members was not evaluated because the members are in compression at all times.

Fig. A5.6. Bending moment sensitivity.

Fig. A5.7. Foundation reaction sensitivity.

A5.6 Conclusions

In the range of foundation modeling parameters evaluated, only the effectiveness of the inner concrete in providing support for the beam has any significant effect on the beam flange stresses. This variable was included in the support beam parametric analysis.

Appendix 6

CALCULATION OF K_I VALUES FOR CRACKS EMANATING FROM
A HOLE IN A PLATE (TROJAN)

Finite-element calculations of K_I values for cracks emanating from a hole in a plate were performed in support of the K_I evaluations discussed in Sect. 6.7.3.1. In this latter section, K_I values were calculated using the model shown in Fig. 6.41 and Fig. A6.1. As mentioned in Sect. 6.7.3.1, the loads are more nearly applied to the flange portion of the beam as shown in Fig. 6.41. The finite-element models corresponding to Figs. 6.41 are shown in Figs. A6.2 and A6.3.

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$2a/W$	$F \left(\frac{2R}{W} = 0 \right)$
0	1.0000
0.1	1.0061
0.2	1.0249
0.3	1.0583
0.4	1.1102
0.5	1.1876
0.6	1.3043
0.7	1.4891
0.8	1.8161
0.9	2.5482

$2a/W$	$F \left(\frac{2R}{W} = 0.25 \right)$
0.25	0
0.26	0.6593
0.27	0.8510
0.28	0.9605
0.29	1.0304
0.30	1.0776
0.35	1.1783
0.40	1.2156
0.50	1.2853
0.60	1.3965
0.70	1.5797
0.80	1.9044
0.85	2.1806
0.90	2.6248

$2a/W$	$F \left(\frac{2R}{W} = 0.5 \right)$
0.50	0
0.51	0.6527
0.52	0.8817
0.525	0.9630
0.53	1.0315
0.54	1.1426
0.55	1.2301
0.60	1.5026
0.70	1.8247
0.78	2.1070
0.85	2.4775
0.90	2.9077

Fig. A6.1. Newman solution for cracks emanating from circular hole in finite rectangular plate subjected to uniaxial stress.

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(a) FULL MODEL

BOUNDARY CONDITIONS		
0 = FREE		
1 = FIXITY		
	X-DISP	Y-DISP
	1	0
	0	1
	1	1
LOADS		
	CONCENTRATED FORCE	
	DISTRIBUTED FORCE	
ΔT	THERMAL LOAD

(b) CRACK-TIP REGION

BOUNDARY CONDITIONS		
0 = FREE		
1 = FIXITY		
	X-DISP	Y-DISP
	1	0
	0	1
	1	1
LOADS		
	CONCENTRATED FORCE	
	DISTRIBUTED FORCE	
ΔT	THERMAL LOAD	

Fig. A6.2. Finite-element model for beam flange with uniform end load. (a) Full model and (b) crack-tip region.

ORNL-DWG 88-5088 ETD

(a) FULL MODEL

BOUNDARY CONDITIONS 0 = FREE 1 = FIXITY		
	X-DISP	Y-DISP
$\rightarrow$	1	0
A	0	1
Δ	1	1
LOADS		
$\downarrow$	CONCENTRATED FORCE	
$\downarrow$	DISTRIBUTED FORCE	
ΔT	THERMAL LOAD

(b) CRACK-TIP REGION

BOUNDARY CONDITIONS 0 = FREE 1 = FIXITY		
	X-DISP	Y-DISP
$\rightarrow$	1	0
A	0	1
Δ	1	1
LOADS		
$\downarrow$	CONCENTRATED FORCE	
$\downarrow$	DISTRIBUTED FORCE	
ΔT	THERMAL LOAD	

Fig. A6.3. Finite-element model for beam flange with line load.

The finite-element and "handbook" values of K_I for the problem depicted in Figs. A6.1 and A6.2 are shown in Table A6.1. As indicated, the agreement is within 0.7%.

A comparison of K_I values for the models depicted in Figs. A6.2 and A6.3 shows that the line-load model results in a 5% higher value of K_I for the range of crack depths considered (Table A6.2). Assuming a critical flaw depth of 0.4 in., based on the model in Fig. A6.1, the required reduction in critical flaw depth to maintain the same critical value of $K_I = K_{Ic}$ is 0.1 in., which is obtained by interpolation of the data in either Table A6.1 or A6.2. A flaw size reduction of 0.2 in. is obtained when the starting critical flaw size is 0.9 in.

Table A6.1. Comparison of "handbook" and finite-element-analysis K_I values for the problem in Fig. A6.1 with $s = 16.43 \times 10^3$ psi

a	2a/w	F	K_I (Newman)	K_I (ORVIRT.PC) ^a	% Diff.
2.25	0.28125	0.9692	42.33	42.35	0.04
2.50	0.3125	1.1028	50.77	51.12	0.68
3.00	0.3750	1.1970	60.37	59.99	0.63
4.00	0.5000	1.2853	74.84	74.95	0.15

^aSee Ref. 1.

Table A6.2. Comparison of finite-element-analysis K_I values for the problems in Figs. A6.2 and A6.3 with equivalent end stresses of 16.43×10^3 psi

Crack depth	K_I (ORVIRT.PC) ^a		% Diff.
	Line load	End load	
0.25	44.61	42.35	5
0.50	53.75	51.15	5
1.00	62.72	59.99	5
2.00	77.66	74.95	4

^aSee Ref. 1.

Reference

1. J. W. Bryson, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., *ORVIRT.PC: A 2-D Finite Element Fracture Analysis Program for a Microcomputer*, NUREG/CR-4367 (ORNL-6208), October 1985.*

*Available for purchase from National Technical Information Service, Springfield, Virginia 22161.

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