# GE RESPONSES

# TO NRC QUESTIONS

# FOR PILGRIM SHROUD REPAIR

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**Pilgrim Core Shroud Repair**
**NRC Questions and Responses**

**Question 1**

*If the Pilgrim plant is to be uprated in power at a future time, how will this affect the design margin for the core shroud repair?*

**Response 1**

At this stage, there is no plan to pursue power uprate for Pilgrim. If at a later date power uprate is attempted, the shroud stabilizer will have to be reanalyzed to demonstrate its adequacy for the power uprate operating conditions.

**Question 2**

*Since the stabilizer assemblies are installed prior to any actual weld failures, the failure of the H2 and H3 welds and the H7 and H8 welds will result in some reduction of the tie rod preload. Provide an evaluation describing how the failure of welds H2, H3, and H7, and H8 have been accounted for in determining the necessary tie rod tensioning to prevent vertical separation of the most adverse combination of failed welds during normal operation.*

**Response 2**

An evaluation of the effect of shroud stiffness on tie rod preload is included in Supplement A to the Pilgrim Shroud Repair Hardware Stress Analysis Report. If cracks presently exist or later develop at the H2/H3 welds on the top guide ring, the vertical stiffness of the shroud is reduced. The thermally induced tension (thermal preload) developed in the tie rods is a function of the shroud stiffness as well as the differential thermal expansion between the shroud and the stabilizer assemblies. It is also known (see Question 23) that at lower shroud stiffness values the mechanical preload is decreased to zero and a net looseness, or gap, occurs when the shroud head is installed during cold shutdown conditions. The most adverse combination of weld cracks and lowest tie rod thermal preload occurs when the tie rods are installed on the shoud with the H2/H3 welds intact and these welds later crack along with failure of the H7 weld at the core plate. This is the bounding case analyzed in Supplement A to the stress report. It is shown that there is a net compression on the shroud under these conditions, therefore no crack separation will occur during normal operation. Furthermore, the value to which the shroud stiffness would have to decrease before separation would occur is determined to be $1.40 \times 10^6$ lb/in which is below even the most conservative lower bounding stiffnesses calculated for the shroud under combined worst case cracking conditions (see Question 26).

The tie rod mechanical preload is not a necessary component of the final tie rod tension that develops during reactor operation. The Pilgrim shroud repair design depends on the thermal preload that develops due to differential thermal expansion between the shroud and the stabilizer assemblies. This thermally induced preload is inherently uniform in its application of loads to the shroud. It is shown that the magnitude of the thermal preload is sufficient to maintain compression during normal operation on any cracked welds under the most adverse combination and sequence of weld cracking.

**Question 3**

*The recirculation line break (RLB) loading greatly fluctuates with respect to time, which will result in significant dynamic amplification of the loads onto the shroud. In the analysis of the RLB loads on the core shroud, have the loads been dynamically applied to the shroud structure? If not, provide an evaluation of the repaired core shroud with dynamically applied RLB loads.*

**Response 3**

The shroud repair stress analysis was performed using dynamic analysis methods, as needed, to produce the peak applied forces for the existing shroud and the new repair hardware. Typical dynamic loads are formed from seismic and LOCA events. The peak dynamic forces are then combined with the static forces, by absolute sum, to obtain the peak loading for finite element analysis or manual calculations to determine peak stresses and deflections.

This approach was used for the recirculation line break (RLB) LOCA and main steam line break (MS) LOCA events. For the MS LOCA, the dynamic load is from the pressure uplift force acting uniformly on the core plate and shroud head. The peak dynamic force was applied as a static load to the shroud and stabilizers. The MS LOCA is the event that is analyzed singularly and in combination with seismic loads in the stress analysis report and supplement.

For the RLB, the dynamic load is from the initial shock wave that travels through the water in the shroud annulus at the speed of sound, referred to as the acoustic load. This acoustic shock wave strikes the shroud from one side and could, thereby, apply a net load to the shroud. However, as discussed below, the excitation from this initial shock wave is at too high in frequency to introduce a significant load on the shroud.

In addition to the acoustic load, there is a transient unbalanced pressure distribution around the shroud that applies a net lateral load. As discussed below, this peak load was considered as a superimposed static load on the shroud. However, the bounding case for the design of the new hardware remains the MS LOCA as presented in the stress analysis report, since it produces the greatest forces and deflections.

The acoustic load acting on the shroud corresponds to the classic case of a low frequency structure being excited by a dynamic load with high frequency content, relative to the structure frequency. If the ratio of the characteristic frequency of the load to the natural frequency of the structure is greater than 2.0, the dynamic response of the structure is deamplified, the greater the ratio the greater the deamplification.

The resultant acoustic load on the shroud typically has a characteristic frequency greater than 50 Hz. The Pilgrim shroud fundamental frequency in the uncracked model without the shroud repair hardware is 8.66 Hz. Furthermore, the shroud fundamental frequency in the cracked model with the shroud repair hardware installed is as low as 0.074 Hz. The

ratio of the acoustic load characteristic frequency to the shroud fundamental frequency is therefore greater than 5.75 for the uncracked case and is as high as 675 for the cracked cases.

The response of the shroud to the acoustic load will be bounded, with high margin, by the response of the same shroud to a continuous harmonic load of many cycles with the same amplitude and same characteristic frequency as the acoustic load. This follows from the fact that such a continuous harmonic load, with many cycles, will allow for vibration buildup.

For the cracked cases, the horizontal mass of the shroud assembly above the crack is supported by only the shroud stabilizer springs and behaves as a simple harmonic oscillator. Consequently, the bounding case for the acoustic load on the shroud corresponds to a Simple Harmonic Oscillator (SHO) subjected to harmonic excitation in which the ratio of the driving frequency to the natural frequency is much greater than 5.75. The dynamic deamplification for the bounding case is therefore less than 0.01. It then follows that the acoustic portion of the RLB load can not excite the shroud to any significant level.

Following the initial acoustic shock load there is a horizontal loading and overturning moment on the shroud which results from the integrated effect of the nonsymetric blowdown pressure distribution in the annulus between the vessel and the shroud. The maximum value of this flow induced loading was considered for the Pilgrim shroud repair. This load is relatively constant for about five seconds and is therefore considered a static load. Because the RLB case does not include the increased differential pressures across the core plate and shroud head that accompany a steam line LOCA, the RLB loading is not a limiting event for stabilizer design.

**Question 4**

*GE-NE-B1100617-03, Rev. 1 states that the main steam line break (MSLB) alone is the only event which causes the core shroud to lose compressive load since the lateral safe-shutdown earthquake (SSE) loading in combination with the loss-of-coolant accident (LOCA) causes the failed shroud sections to remain in contact. However, the lateral SSE loading will cause tipping which will result in separations at least on one side of the shroud. Provide the maximum transient vertical separations of the failed shroud welds for all plant transient and accident events or combinations thereof.*

**Response 4**

During SSE and OBE loading, there will be tipping toward one side of the shroud (say 0° azimuth). On the opposite side (180°) there will be separation if a weld has completely cracked. The worst case event would be a combination of an SSE plus a MSLB LOCA. For analysis purposes, an absolute summation of the maximum vertical seismic, seismic overturning moment and LOCA uplift forces were assumed. This is a conservative combination which results in a momentary separation at the 180° azimuth of 0.494 inches

at the H-10 weld. Any other combination of cracked welds and forces would result in smaller gaps.

**Question 5**

*For the limiting vertical separations at the various weld locations, do the stabilizers restrain all of the loads, or do the control rod drive guide tubes, core spray piping, or any other reactor internal structures restrain some of the transient loads? Provide an evaluation of these safety components to assure their structural integrity and to assure they remain capable of performing their safety functions. Also, following the separation of the shroud during emergency and faulted events, the upper portion above the various failed welds will impact the lower portion of the shroud. Especially for the SSE where the core shroud tips, the seismic loading is applied and is removed very quickly such that the tie rod (and gravity) forces will snap the core shroud back into position. Provide an evaluation of the kinetic energy of the moving mass and its effect on the structural integrity of the shroud, fuel, control rods, reactor vessel and any other safety component.*

**Response 5**

The shroud repair hardware is designed to restrain all of the loads for both steady state and transient conditions. Load paths through the Core Spray piping and CRD guide tubes do not provide significant restraint of shroud dynamics. Specifically, the CRD guide tubes are designed to accommodate a 1/2 inch vertical displacement without restraint of motion, before loading up. The vertical displacements predicted with the repair hardware in place are all less than this value at the CRD guide tubes. For lateral motion, the guide tubes are modeled in the dynamic analyses.

The core spray line (CSL) is a run of 5 inch pipe which conducts core spray flow from the RPV nozzle thermal sleeve to the shroud. The CSL does not provide significant restraint to the shroud, in fact the CSL was specifically designed with flexibility to accommodate the relative thermal expansion of the shroud relative to the RPV. Shroud cracking has been shown to result in larger end-to-end seismic displacement (anchor movements) for the CSL than that considered for the original CSL design. The larger end-to-end OBE displacement for an assumed all welds cracked case was the subject of an analysis completed for another plant where the CSL design is similar to Pilgrim's, and the end-to-end displacement is larger. This analysis, using ASME Section III Subsection NB piping rules as a guide demonstrated compliance with fatigue requirements for normal and upset events including ten cycles of OBE seismic. Since the primary plus secondary stress range (equation 10, NB-3653) exceeded 3Sm the simplified elastic plastic method of NB-3653.6 was applied. The stress resulting from end-to-end CSL displacement for one cycle of steam line break LOCA plus SSE is classified as secondary and is therefore not required by Section III to be evaluated. However as a functional check it was shown that the maximum strain in the CSL during this faulted event is less than one percent which is well below the minimum 25 percent ultimate strain for the 304 stainless steel piping material specification.

The displacements on the Pilgrim CSL are approximately 15 percent less than for this similar plant CSL. The configuration is similar, and the piping is the same size and material. Since there was shown to be such a large margin to failure (less than one percent

strain versus the minimum 25 percent ultimate strain) the acceptability of the Pilgrim CSL response is assured without further consideration.

It should be noted that the transient gaps in the shroud are relatively minor (see Question 4 above), and their opening and closing will not result in substantial dynamic amplification. Gaps of this magnitude are typically evaluated using linear dynamics with conservative assumptions. Although the shroud mass is substantial, the gap area is also large, and the volumetric strain energy due to these small gap closures will be minimal. Thus, for the seismic events, these gaps were treated using linear dynamic analysis. Parametric seismic analyses were performed to bound the effect of the small vertical gaps. Cracks were modeled as both a hinge and as a roller.

During a main steamline LOCA event, the upper most fully cracked weld will open as identified in the stress analysis report, due to an increase in the shroud head pressure. Once this pressure peaks, it ramps back down in a manner which will lower the head and close the gap slowly (in terms of impact loads), such that the impact energy will be small. This faulted condition gap closure is not considered to result in impact loads requiring specific analysis, due to this resisted closure under a pressure down ramp. In this instance, the gap is modeled as a roller condition for the lateral seismic analysis.

Dynamic and static testing demonstrated the operability of the CRD system with simulated motion of the core support plate and top guide; these motions were in excess of the motions predicted at Pilgrim (see GE-NE-771-44-0894, Rev. 2). Therefore, the Pilgrim CRD system will be operable.

**Question 6**

*GE-NE-B1100617-03, Revision 1 states that there are no permanent radial deflections resulting from any of the required design conditions. However, the assumed analysis models of either hinges or rollers at the crack interfaces would not appear to conservatively model possible permanent radial displacements since any friction between sliding sections of the shroud would prevent the radial springs from entirely pushing the sections back into place. Provide an evaluation of the resulting radial deflections with frictional sliding at the crack interfaces.*

**Response 6**

The design of the repair hardware shroud stabilizer springs is governed by: (i) the maximum allowable relative displacement between the vessel and the shroud at the spring locations, (ii) the stabilizer spring stiffness, and (iii) the maximum spring loads. Specifying any two of the three will automatically define the third. Consequently, given the spring stiffness, it is only necessary to obtain an upper bound on the relative displacements to calculate the design loads and complete the design of the stabilizer springs. The assumption of the hinge and roller conditions in the linear seismic analyses yields upper bound values of the spring deformations required for design.

It is true that the shroud weld cracks exhibit nonlinear properties (friction, gap, local stiffness at weld crack interface, etc.) at the weld crack interfaces. However, if these nonlinear properties were modeled and commensurate nonlinear analyses performed, this would result in significant attenuation or reduction in the calculated peak relative displacements (hence loads) used to verify the design of the stabilizer springs.

Any permanent net lateral relative displacement between the shroud and the vessel will not effect the structural design of the stabilizer springs. The only concern would be if the net lateral displacement were sufficiently large to alter the core flow because of leakage. The maximum calculated transient horizontal displacement of the core support plate is 0.7 inches and intermediate cylinders of the shroud are limited to 0.75 inches displacement by physical stops. These displacements are all no more than one half the thickness of the shroud ensuring that leakage is minimal.

**Question 7**

*It is stated that the component stress evaluations are determined from the dynamic analysis of the horizontal seismic loads in combination with the vertical seismic loads. How were the horizontal and vertical loads combined (i.e., by absolute summation or by another method), and how were they combined according to the original plant design basis? Similarly, how were the SSE and LOCA dynamic loads combined for the two faulted service conditions, and how were they combined in the original design of the core shroud?*

**Response 7**

There are three possible collinear contributions for each seismic load. One for each of the two horizontal components of seismic excitation and one for the vertical. For the Pilgrim shroud repair hardware design, the maximum contribution from either of the two horizontal seismic analyses was combined absolute sum with the corresponding peak vertical contribution for each seismic load "See GENE-771-79-1194". This method for combining peak collinear contributions due the three orthogonal spatial components of excitation is identical to the original (and existing) licensing design basis for Pilgrim.

Also, commensurate with the primary structure being vertically rigid with respect to the vertical seismic free-field input motion, the Pilgrim seismic licensing design basis necessitates only horizontal dynamic analyses. Consequently, in conjunction with existing geometric symmetry in the primary structure, the peak horizontal components were obtained from separate N/S and E/W horizontal analyses and the peak vertical contributions were from static dead weight analyses.

The peak collinear contributions due to the SSE and LOCA dynamic loads were combined by absolute sum for the Pilgrim shroud repair hardware design. It is not clear how they were combined in the original design of the core shroud. However, the absolute sum methodology presently used is the most conservative method employed for nuclear application.

**Question 8**

*The analysis of the two faulted load combinations of SSE + LOCA involves a linear-elastic analysis method. However, the core shroud and repair stabilizer structure is not one having linear stiffness characteristics. There are gaps in the failed shroud structure which affect not only the mass continuity but the stiffness of the shroud as well. When the gaps are closed, the shroud is a continuous structure in compression, but when the gaps open, there is no stiffness of the shroud above the gaps. Provide an evaluation of the effects of the structural gaps and any other structural nonlinearities that can affect the analysis results.*

**Response 8**

The primary source of the nonlinearities associated with the shroud repair hardware evaluation is the assumption of 360° through-wall cracks at the core shroud horizontal welds. Consequently, as discussed below and depending on the excitation level, the shroud itself may respond in a nonlinear manner during dynamic excitation. Also, the individual shroud repair hardware components (stabilizer springs and tie rods) remain linear in the range of the calculated seismic responses. This is based on actual load/displacement curves for the components. However, the primary structure rotational stiffness due to the tie rods is highly dependent on the location of the axis-of-rotation used to calculate the stiffness. The location of the axis-of-rotation continually changes depending on the level of the dynamic excitation. In addition, only horizontal seismic analyses are performed for the shroud repair hardware evaluation. Consequently, the nonlinearities in question are horizontal in nature and, in addition to the continually changing-axis-of rotation of the rotational spring, are due to gaps and mechanical interferences which develop at the weld crack interfaces during dynamic excitation. The gaps and mechanical interferences at the weld crack interfaces alter the shroud stiffness and may result in linear, bi-linear, and tri-linear stiffness characteristics in the horizontal primary structure model.

When a weld crack gap is closed, and assuming an adequate normal force at the weld crack interface to develop mechanical interference, the horizontal stiffness in the shroud is commensurate with a "pinned" or "hinged" condition at the weld crack interface. Under this condition the shroud cannot transmit a moment through the weld crack interface; however, the primary structure behaves in a linear manner. The responses from the linear pinned analyses will bound the corresponding nonlinear responses which result when the weld crack interfaces vary between pinned and roller conditions with time.

When the weld crack gap is open and the dynamic excitation is not sufficient to close the gap, the only seismic load path into the portion of the primary structure model above the gap is through the shroud repair hardware springs. For this condition, the shroud horizontal stiffness is commensurate with a "roller" shroud connectivity condition at the weld crack interface. The shroud cannot transmit either shear or moment across the weld crack interface. Again the primary structure model behaves in a linear manner if the dynamic loading is below a level such that the gap does not close during excitation.

During actual dynamic excitation, the shroud connectivity condition at each weld crack is continually varying between a pinned and a roller condition with time. It then follows that the dynamic characteristics of the primary structure seismic model also varies with time

depending on the actual weld crack conditions which continually fluctuate between being pinned or roller. For Pilgrim, the shroud fundamental is as low as 0.074 Hz for the worst weld crack configuration as compared to 8.66 Hz for the uncracked case with no shroud repair hardware.

As indicated above, nonlinear behavior results if either: (i) a gap is closed and there is not sufficient preload to prevent it from opening during dynamic excitation, or (ii) the gap is open and the dynamic loading is sufficient to close the gap during excitation. These two conditions can exist during actual dynamic excitation. Corresponding, nonlinear analyses, with the gap and friction characteristics appropriately modeled, will result in significant changes in the frequency content of the calculated loads. However, the nonlinear analyses will not yield the bounding values for the peak loads (i.e., maximum spring deformation or maximum spring load) required to demonstrate the seismic design adequacy of the shroud repair hardware. These maximum values are conservatively calculated from the linear primary structure seismic analyses corresponding to the bounding "pinned" and "roller" shroud connectivity configurations at the shroud weld crack interfaces. The bounding, linear analyses also account for the controlling values of the rotational stiffness due to the tie rods.

The objective of the shroud repair hardware seismic analysis is to demonstrate the seismic design adequacy of the repair hardware. To do this only bounding loads are required. Therefore, it is not required to obtain precise values for the nonbounding, nonlinear responses at each point in time.

**Question 9**

*Describe how uncertainties are accounted for in structural modeling for the time history analyses, similar to peak broadening for a response spectrum analysis.*

**Response 9**

The purpose in addressing the effects of parameter variations is to account for uncertainties in the calculated primary structure natural frequencies due to uncertainties in such parameters as material and soil properties, structural damping and soil damping, soil-structure interaction techniques, geometrical dimensions and approximations inherent to dynamic modeling and analysis. The concern is the effect the parameter variations will have on the computed floor response spectra generated from time history analyses of the primary structure since these spectra will be the input for the dynamic analyses of safety related secondary systems.

The current NRC acceptance criteria for the consideration of the effects of parameter variations are provided in Subsections II.9 and II.5 of Standard Review Plan (SRP), Section 3.7.2, "Seismic System Analysis", Rev. 2 - August 1989. The Pilgrim seismic licensing basis does not require compliance with the current NRC acceptance criteria contained in this SRP. Per the SRP, one acceptable approach is to smooth the computed floor response spectra obtained from the primary structure time history analyses and broaden the spectral peaks that are coincident with the structural natural frequencies. This corresponds to the approach referred to in Question 9.

Also, from Subsection II.5 of SRP 3.7.2, it is acceptable to use a "single" artificial input time history (without expanding or contracting the time history time step) in the primary structure seismic analysis.

However, as indicated in the SRP subsection, the use of a single artificial time history should also be justified as outlined in Subsection II.1.b of SRP Section 3.7.1. From SRP 3.7.1, "when a single artificial time history is used in the design of seismic Category I structures, systems, and/or components, it must in general satisfy requirements for both enveloping design response spectra as well as adequately matching a target PSD function compatible with the design response spectra. Therefore, in addition to the response spectra enveloping requirement, the use of a single time history will also be justified by demonstrating sufficient energy at the frequencies of interest through the generation of PSD function, which is greater than a target PSD function throughout the frequency range of significance."

Single time histories (i.e., no expansion or contraction of the Δt time step) were used in the primary structure time history analyses for the seismic design adequacy evaluation of the shroud repair hardware. Single time history analyses were performed using both the natural Taft earthquake and a Housner synthetic time history. All requirements for enveloping the smoothed designed response spectra were satisfied in the generation of the Housner synthetic time history. However, the Pilgrim seismic licensing design basis specified in the current Pilgrim FSAR does not contain the requirement to match a target PSD function. Also, a target PSD function was not available corresponding to the Housner free-field input motion spectra. However, to ensure adequate energy content in the low frequency range (i.e., down to 0.074 Hz for analysis of the bounding shroud weld crack configurations) the duration of the Housner synthetic time history was increased to 40 seconds and the Strong Motion duration to over 20 seconds. Also, adequate enveloping of the smoothed Housner target spectra by the corresponding spectra from the generated Housner synthetic time history was demonstrated for frequencies from 0.03 Hz to 50 Hz. The smoothed Housner spectra from 0.03 Hz to 50 Hz were used to generate the Housner horizontal synthetic time history.

Finally, it is noted that only the peak dynamic loads from the Pilgrim primary structure time history analyses are applied to perform secondary, static stress analyses of the shroud and shroud repair hardware. Therefore, the Housner, free-field, synthetic time history input motion to the Pilgrim primary structure must be adequate to engender bounding peak responses only since there is no dynamic amplification in the static stress analyses performed for the secondary systems.

**Question 10**

*GE-NE-B1100617-03, Rev. 1 discussed the increased carryunder affect on jet pump cavitation margin. Provide the analysis that the demonstrates that the jet pump cavitation margin remains adequate and will not cause any unreviewed safety questions.*

**Response 10**

The bounding condition for minimum jet pump cavitation margin is at 100% power with maximum flows (107.5%). At lower flow rates, the design margin to cavitation increases. At 75% flow, this design margin increase is significantly larger than the slight

reduction in the design margin caused by the increased carryunder. The net result is that margin to cavitation remains acceptable.

At 100% rated power and 75% rated core flow, the predicted leakage has the effect of increasing the carryunder by 0.02% above the design value of 0.25%. At the 75% core flow condition, the slight increase in carryunder above the design value results in a slight decrease in subcooling (about 0.1 Btu/LB) relative to the design condition. This slight reduction in subcooling, in turn, causes a slight reduction in jet pump cavitation margin relative to the design condition. However, the reduction in core flow compared with the rated (100%) core flow condition results in a much larger increase (about 5 Btu/LB) in subcooling relative to the rated (100%) condition. Therefore, the subcooling at 75% core flow still exceeds the subcooling at rated (100%) core flow and is adequate. The bounding condition with minimum subcooling (and minimum cavitation margin) is the maximum core flow condition (107.5 %) rated, where the design carryunder value is met. Hence, the jet pump cavitation margin remains adequate and does not result in an unreviewed safety question.

**Question 11**

*At increased carryunder from the induced shroud head leakage, the combined effective carryunder slightly exceeds the design criteria at 75% rated core flow. While operating at 75% rated core flow, what effect does exceeding the carryunder design criteria have on jet pump integrity, emergency core cooling system ECCS performance, and NPSH for the recirculation pumps?*

**Response 11**

The maximum core flow condition is the bounding condition for jet pump integrity. Since the design carryunder value is met at the maximum core flow condition, there is no impact on jet pump integrity. The minimum NPSH for the recirculation pumps also occurs at the maximum core flow condition. The maximum core flow condition bounds the 75% rated core flow condition with respect to NPSH for the recirculation pumps even when the leakage effects are taken into account (as discussed in Response 10). The NPSH margin for the recirculation pumps at rated power and 75% rated core flow remains greater that 500 ft. Hence, there is also no impact on the bounding NPSH for the recirculation pumps.

The impact of slightly increased carryunder (0.02% above the design value at 75% rated core flow) on ECCS performance was evaluated. These results show that for a small decrease in core inlet subcooling (0.1 Btu/lb as a result of the small increase in carryunder above the design value) the peak cladding temperature is decreased. Hence, there is no impact on the bounding ECCS performance as a result of the slightly increased carryunder condition. For the net impact of all leakage and carryunder effects on ECCS performance see the safety evaluation section 1.4.4 (GE-NE-B1100617-03).

**Question 12**

*Provide the ECCS performance analysis with respect to the increased carryunder during the limiting LOCA event to show that 10CFR 50.46 is not exceeded.*

**Response 12**

The analysis of ECCS performance with respect to increased carryunder during the limiting LOCA event is presented below.

The impact of slightly increased carryunder on ECCS performance was evaluated. These results show that for a small decrease in core inlet subcooling (as a result of a small increase in carryunder due to the predicted leakage) the peak cladding temperature (PCT) is decreased. For the rated power and 75% rated core flow condition, the PCT would decrease about 0.4°F as a result of increasing the carryunder by 0.02% above the design carryunder value. This favorable effect results from the decreased subcooling which in turn decreases the break flow rate. Hence, there is no impact on the bounding ECCS performance as a result of the slightly increased carryunder condition. For the net impact of all leakage and carryunder effects on ECCS performance see the safety evaluation section 1.4.4 (GE-NE-B1100617-03).

**Question 13**

*Provide the analysis of the downcomer flow characteristics with the four stabilizers installed. Specifically, address the available flow area in the annulus, the associated pressure drop, and the impact on reactor coolant level, recirculation flow, and ECCS performance.*

**Response 13**

The closest distance between the jet pump suction nozzle inlet (at elevation 307.1 in., where jet pump suction flow enters the jet pump) and the 3.5-in. diameter stabilizer tie rod is 6.37 in.. At this distance the predominately downward flow distribution near the jet pump nozzle will not be significantly affected.

The smallest vessel-to-shroud annulus plan area (downcomer flow area) is above the top guide ring area of the shroud (44.5 in. above the jet pump suction nozzle inlet) as shown in figure 13-1. This flow area, based on the as-built vessel diameter*, and the area of items which block this annulus is summarized in the table below. Based on this table, the four added upper stabilizer springs and their supports block about 5 percent of pre-repair minimum downcomer area. This blockage applies only to the short vertical distance corresponding to the length of the upper stabilizer springs and their supports, located between welds H1 and H2. The additional horizontal flow blockage from shroud stabilizer hardware at other elevations in the shroud-to-vessel annulus will be less than this area. The impact of the additional flow blockage on the recirculation system loop hydraulic resistance, loop pressure drop, and the coolant flow rate is estimated to be negligible.

| | |
|---|---|
| Gross annular area (227 in. vessel ID and 195.5 in. shroud OD)*. | 10,453 $in.^2$ |
| 48 - 3.75 x 3.88 in. shroud head bolts and lug sets (conservative) | 698 $in.^2$ |
| 4 - 7.25 in. OD core spray line riser couplings and the short horizontal run to the shroud OD. | 288 $in.^2$ |
| 2 - 13.25 x 8.00 guide rod brackets (on shroud) | 212 $in.^2$ |
| 2 - 2.00 x 7.00 in. shroud lifting lugs | 28 $in.^2$ |
| Net annulus flow area, before shroud repair | 9,227 $in.^2$ |
| 4 - upper stabilizer springs with their supports (see Figure 1) | 476 $in.^2$ |
| Post-repair net annulus flow area | 8,751 $in.^2$ |

* Many system performance calculations were originally based on the 224 in. minimum specified RPV ID. The flow area using the as-built 227 inch diameter as opposed to the minimum 224 is 1,063 $in^2$ which is greater than the 476 $in^2$ blocked by the four stabilizer upper spring assemblies. Thus most previous flow calculations are still conservative with respect to flow area and velocity.

During a recirculation suction line break there may be a significant horizontal component of flow in the lower vessel annulus. The four lower stabilizer springs are each located between jet pumps 45 degrees away from the recirculation outlet nozzle. The vertically oriented flow blockage area of the lower spring assembly is shown in Figure 13-2. By inspection the net vertical flow area at lower stabilizer spring locations is significantly greater than the net area at each of the 20 jet pump diffusers. Thus the blockage effect of the lower springs will have an insignificant effect on recirculation line break blowdown calculations. Hence, ECCS performance is not impacted as a result of the flow blockage associated with the stabilizer mechanisms.

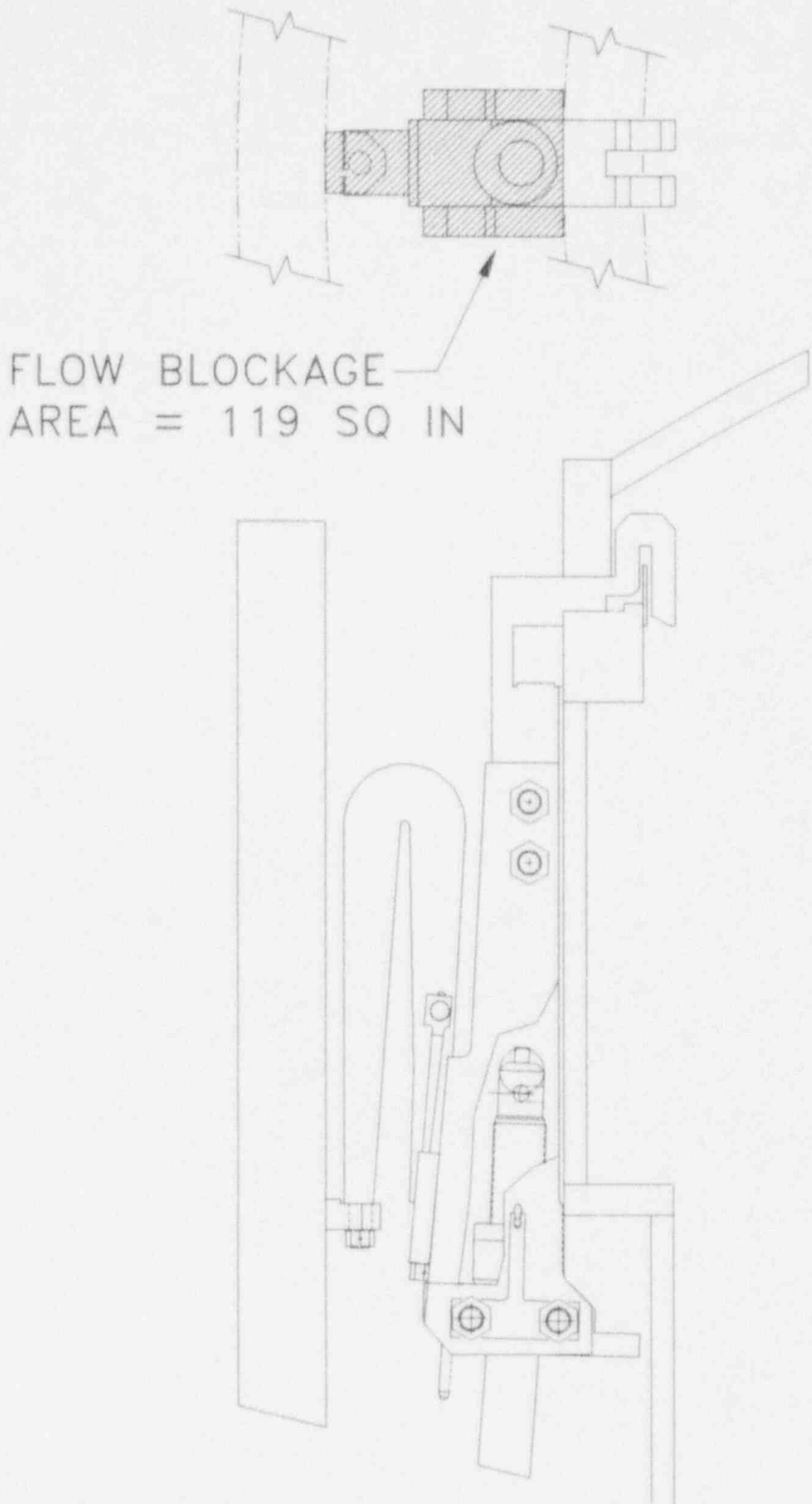


Figure 13-1. Flow Blockage In The Upper Downcomer Area

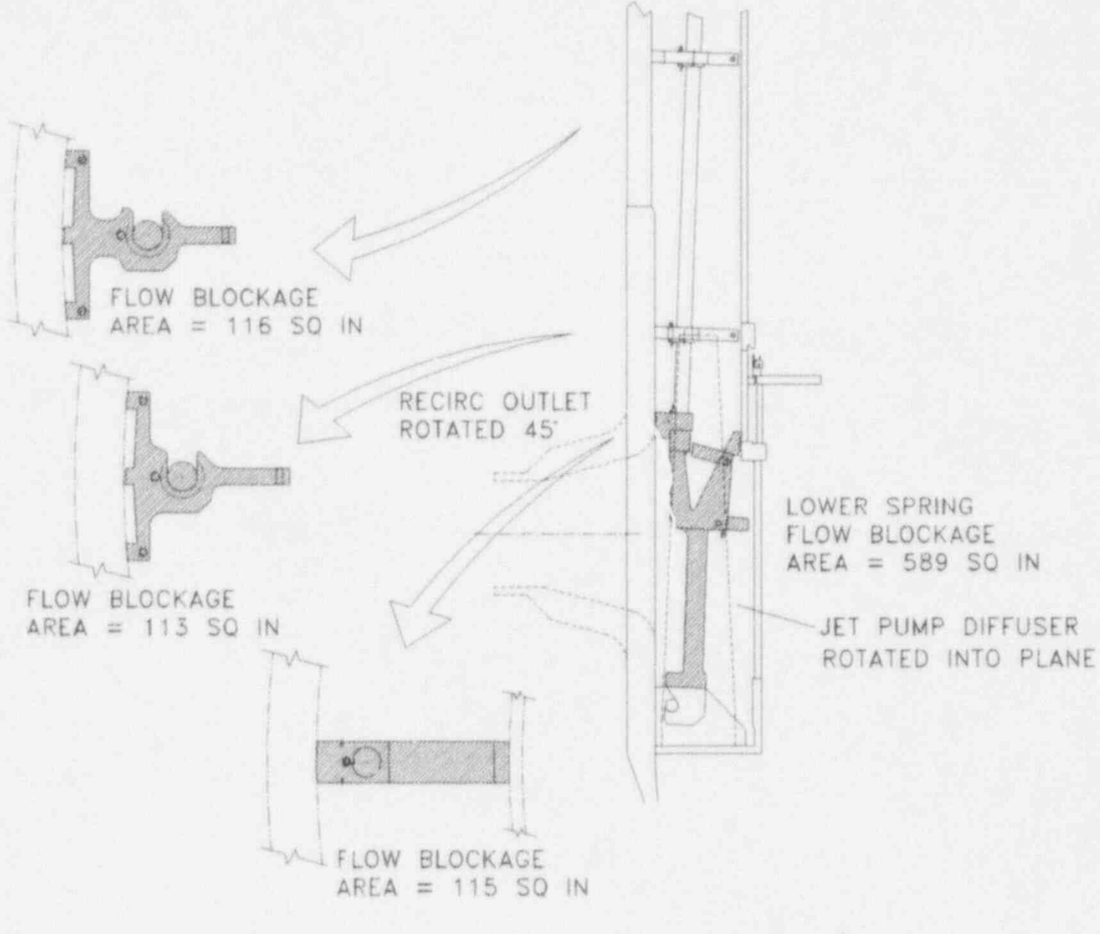


Figure 13-2. Flow Blockage In The Lower Downcomer Area

**Question 14**

*Boiling Water Reactor Vessel and Internals Project (BWRVIP) has issued the following documents to provide guidelines for visual examination (VT) and ultrasonic examination (UT of core shrouds: (a) BWRVIP, "Standards For Visual Inspection of Core Shrouds," September 8, 1994, and (b) BWRVIP Core Shroud nondestructive examination (NDE) Uncertainty & Procedure Standard, November 21, 1994. The guidelines in these documents should be followed in the examination of the core shroud and repair assemblies. The subject BWRVIP documents should also be referenced in the appropriate examination specifications.*

*The staff notes that in Section 4.0 of Repair Examination in the field disposition instruction (FDI) (0228-78003, Revision C) the required resolution for the television camera is defined as capable of resolving a 0.001 inch wire on a neutral gray background. This requirement should be changed to be consistent with the required resolution of a 0.0005 inch wire as recommended in the above referenced BWRVIP document for visual examination of core shrouds.*

**Response 14**

FDI 0228-78003, Section IV, REPAIR PROCEDURE, Paragraph 1.0 identifies the examination method for "welds" to be "Enhanced VT-1". Camera resolution of a .0005 inch wire is required for Enhanced VT-1.

Section IV, Paragraph 4.0, REPAIR EXAMINATION, is referring to examination of the installed hardware at various points by a camera capable of resolving a .001 inch wire.

It is the opinion of GENE that the .0005 inch wire resolution should be used for examination of the welds, but the .001 inch wire resolution is sufficient for examination of the installed hardware at the specified location.

Rather than reference master documents, such as BWRVIP reports, the repair procedures call out applicable specific requirements to be followed during the installation of the stabilizers at Pilgrim.

**Question 15**

*Please discuss the mitigation methods that you plan to apply to the machined threads such as re-solution annealing to minimize the cold work effect. Please also describe how the methods were qualified and the details of controls for application.*

**Response 15**

The XM-19 material used for the tierods is procured in the solution annealed condition. The machined threads on the tierods are not solution annealed after the machining of the threads. (See response to Question 29).

General Electric has been specifying and using XM-19 components in commercial power reactors since the mid-1970s, especially for parts which must be stainless steel and also must be nitrided. The nitriding process involves holding the parts in a chlorine-rich nitrogen environment at 1060° to 1100°F for 16 to 24 hours. Only XM-19 is able to

withstand this treatment without becoming sensitized to Intergranular Stress Corrosion Cracking (IGSCC). Types 304, 304L, 316, and 316L quickly fail. Therefore it is not necessary to remove or anneal any "IGSCC-prone" surface layers after maching, because the material is inherently resistant to IGSCC, even in the as-machined condition.

GE has access to test results which show that XM-19, in BWR environments, is more corrosion and crack resistant in tight crevice situations than 316L or other 300-series stainless. To cite an example; a set of tensile specimens, each bearing a sleeve to create a crevice at the specimen surface, was loaded to 120% of yield in 1981 and that load maintained for the duration of the test. Today, 14 years later, none of the XM-19 specimens has failed or even cracked. Thus, concerns about the durability of XM-19 in threaded joints because of "crevices" are not valid.

All in all, XM-19 is an excellent material for use in BWR applications. It may safely be used in the as-machined condition; it may safely be used in threaded and other highly-creviced geometries, and is superior to 316, 316L, and other alloys for the intended application. Its only drawback is cost and availability - it is not as commonly found in supplier's inventories as the more traditional materials.

**Question 16**

*In the safety evaluation for installation of stabilizers (GE-NE-B1100617-03, Rev. 1), General Electric stated that a minimum of 0.030 inches will be removed from Alloy X-750 materials after the last exposure to acid pickling or high temperature annealing as a control of intergranular attack (IGA). Will this process or any other process be applied to components made of Type XM-19 and Type 316 stainless steel after pickling or annealing to ensure there is no pitting or IGA? Please provide the test data to support that the removal of 0.030 inches surface material would effectively eliminate the pitting or IGA effect resulting from the pickling or high temperature annealing.*

**Response 16**

Each heat and heat-treat lot of raw material is required to pass General Electric Test Procedure E50YP11. In this procedure, a sample of the material is removed and examined in cross section under a microscope, specifically to discover pits or intergranular attack resulting from prior manufacturing processes. Paragraph 4.4.1 states: "Material having IGA in excess of 0.001-inch deep shall be unacceptable." This is 1/30 the amount required to be machined off after the material is accepted by GE.

No pickling is done during manufacture of the shroud stabilizer parts and assemblies, nor is exposure to any type of acid or acidic substances permitted.

There is no known test data which directly attributes pitting or similar surface attack to heat treatment by and of itself. Therefore, the test data in support of GE's position is that there is none to show otherwise.

**Question 17**

*Please identify all the threaded areas and locations of crevices and stress concentration in each component of the core shroud repair assemblies. In the planning of inservice inspection, those areas should be emphasized for inspection because these areas are most*

*susceptible to stress corrosion cracking. Please provide this information in tables and supplement it with sketches.*

**Response 17**

All threaded and crevice areas in the stabilizer hardware are identified in the table below and in the six accompanying figures. One might debate the question of whether any threaded member also creates a crevice, but as all threaded areas are identified these are not identified again as crevice areas. Areas identified as crevices all have some associated mechanical cold work, such as a deformed locking pin. All calculated stresses for steady state normal operation are less than 50 percent of the allowable; thus there are no areas where stress should influence inspection planning. There are no welds in any shroud stabilizer hardware, and no welding is allowed for the installation of this hardware. See also the response to questions 19 and 25 below.

Table 17-1 Summary of Crevice & Threaded Locations in the Stabilizer Hardware

| Figure Number | Part or Assembly Nomenclature | Crevice & Threaded Area Identification<br>*Tn = Threaded Area*<br>*Cn = Crevice Area* |
|---|---|---|
| Figure 17-1 | Upper Stabilizer Assembly | C1, T1<br>C2, T2<br>C3, T3<br>C4, T4 |
| Figure 17-2 | Stabilizer Support Assembly | T5<br>T6<br>C5, T7<br>C6, T8<br>C7, T9<br>C8, T10<br>C9, T11<br>C10, T12 |
| Figure 17-3 | Tie Rod Assembly | C11, T13<br>C12, T14 |
| Figure 17-4 | Tie Rod & Lower Spring Assembly | C13<br>C14<br>C15<br>C16, T17<br>T15<br>T16 |
| Figure 17-5 | Core Plate Wedge Assembly | T18 |
| Figure 17-6 | Shroud Restraint Assembly | T19 |

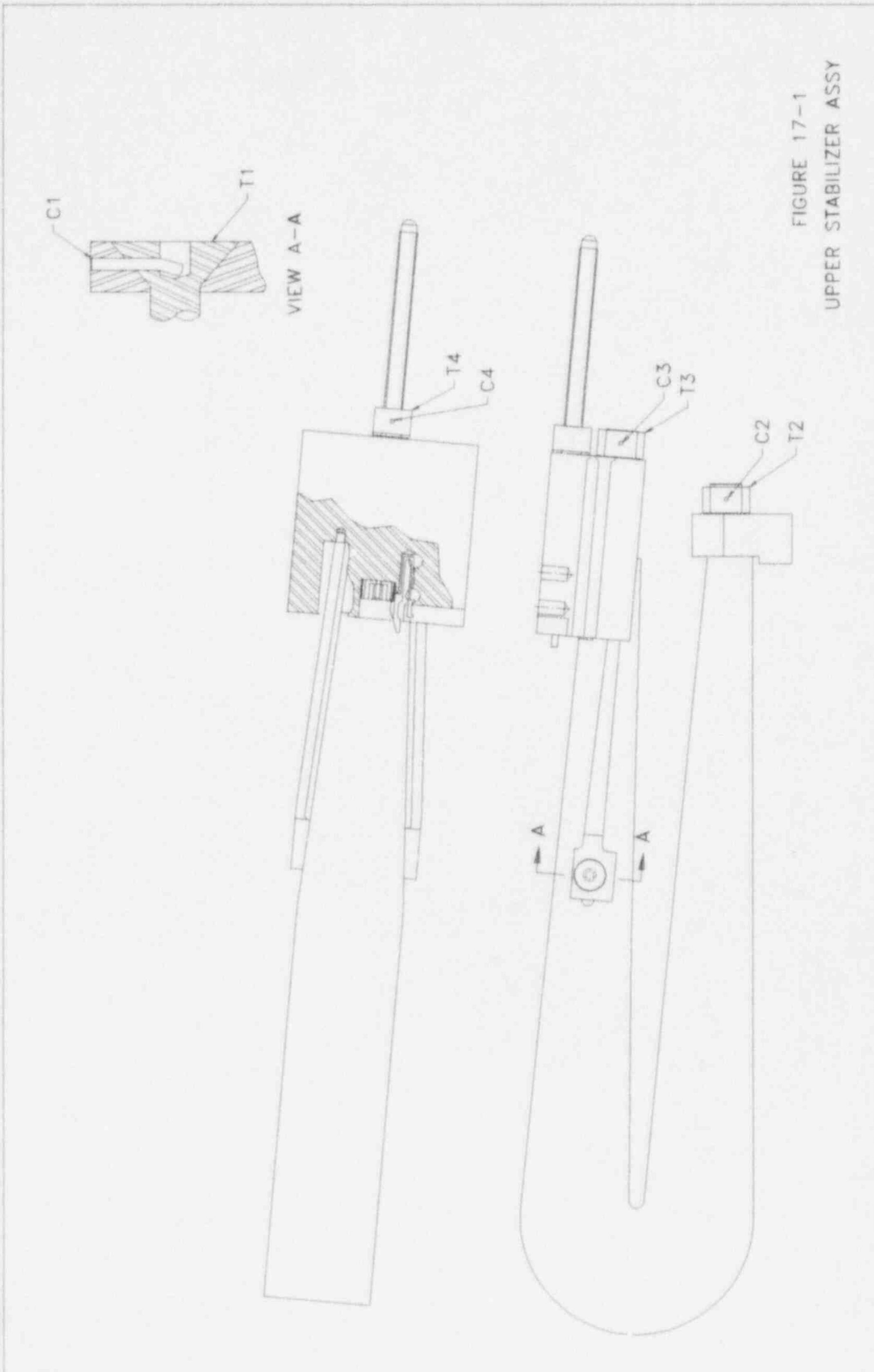


FIGURE 17–1
UPPER STABILIZER ASSY

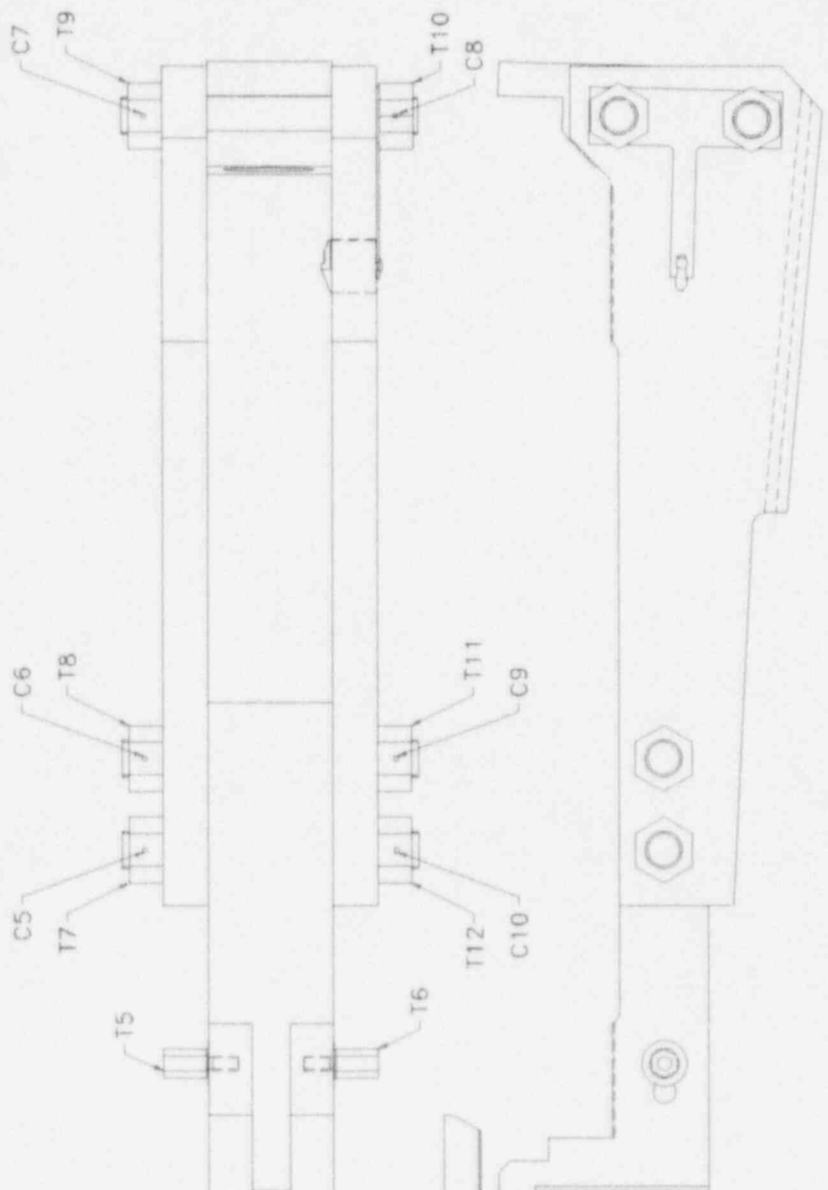


FIGURE 17-2
STABILIZER SUPPORT ASSY

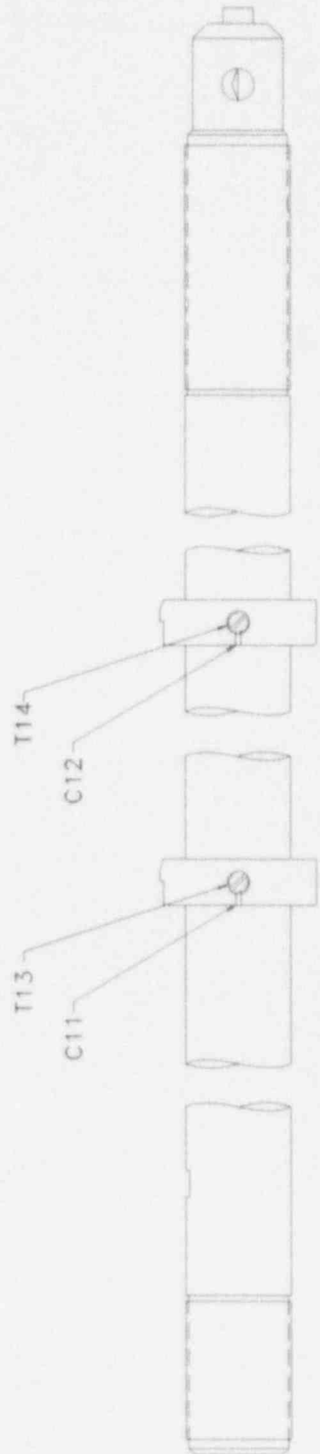


FIGURE 17-3
TIE ROD ASSY

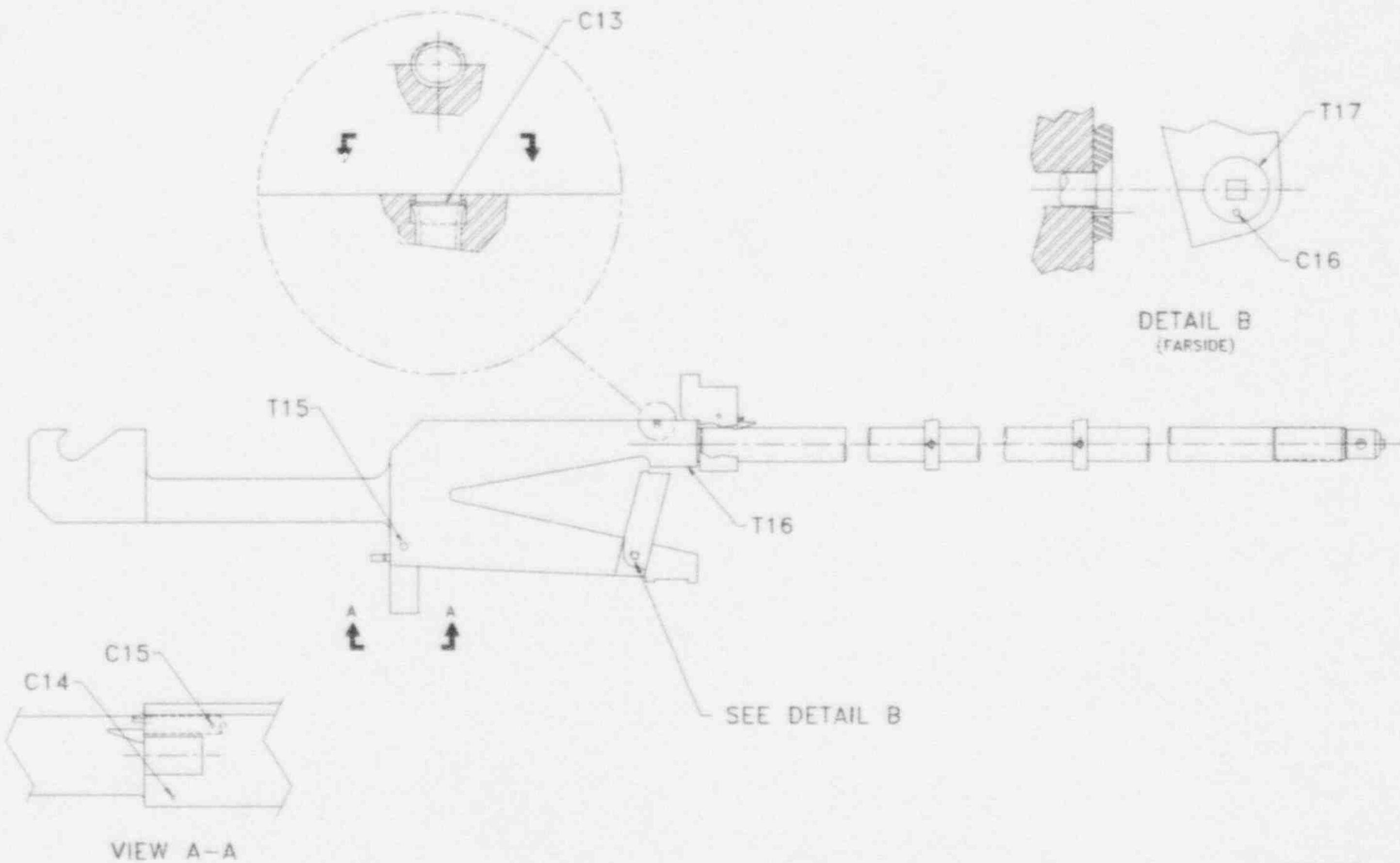


FIGURE 17-4
TIE ROD / SPRING ASSY

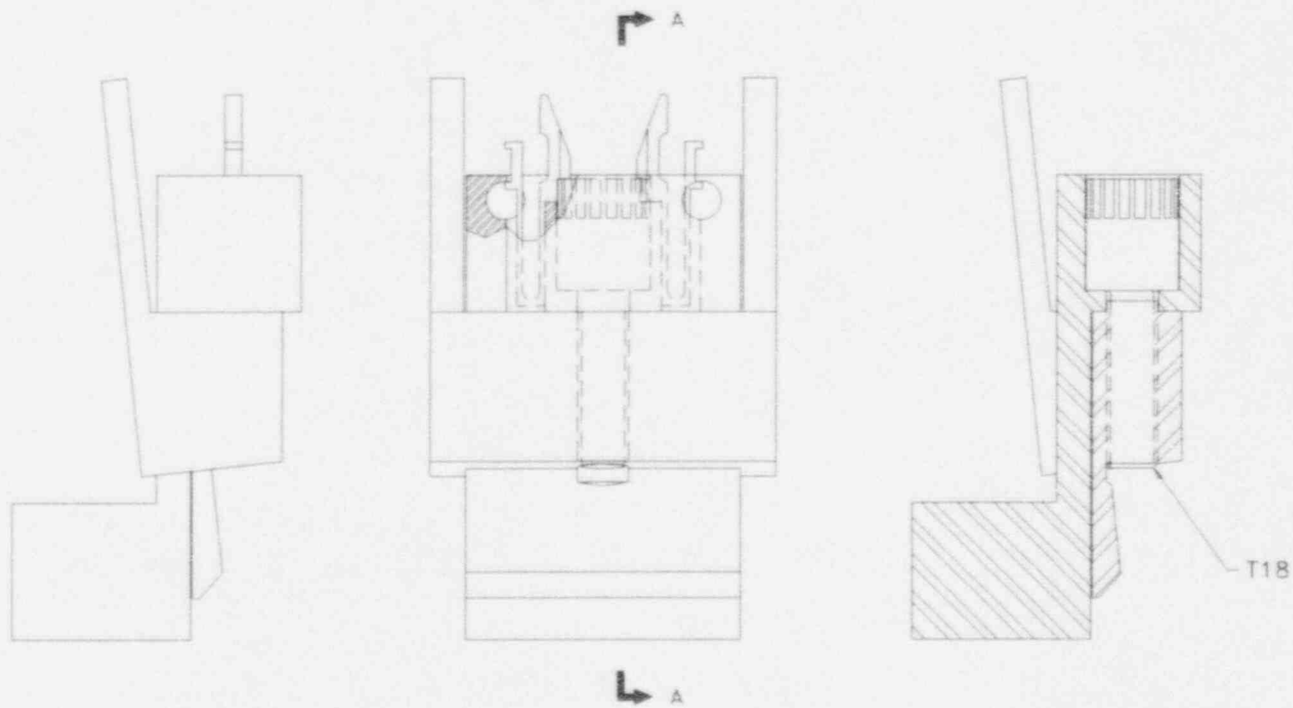


FIGURE 17-5
CORE PLATE WEDGE ASSY

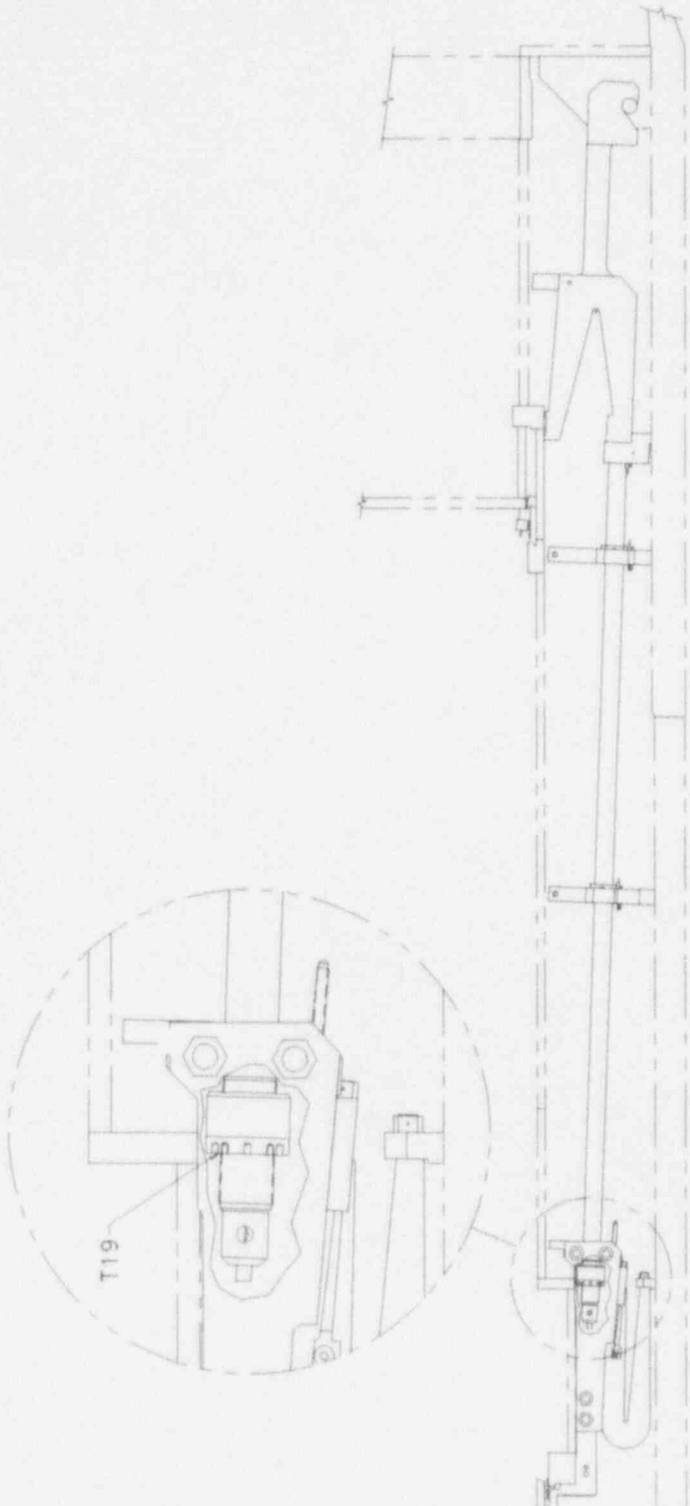


FIGURE 17-6
SHROUD RESTRAINT ASSY

**Question 18**

*Please provide details of your controls in the practices of machining, grinding and threading to minimize the effect of cold work, such as amount of materials to be removed in each pass, application of coolant and sharpness of the tool.*

**Response 18**

Each manufactured piece has its own specific requirements when it comes to "how much" material is removed per pass and which machine is doing the work. Generally speaking, parts are "rough machined" down to within .100" of final dimensions. Then the final clean up (about .010") pass skims off this rough surface to achieve the required size and surface finish. If a tool is dull, then the 125 rms surface finish which is required on all drawings would not be produced. A dull tool produces a smeared or torn surface appearance which is the primary method of monitoring the adequacy of the tooling and the machining process in general. A number of tests on various parts in various heat treated conditions has demonstrated that the most severe operation in terms of surface cold work is generation of the stub ACME threads on the tie rods (See Question 15), which serves as a "worst case" bounding condition.

The judgment and experience of the machinist is relied upon to determine how much material can be safely removed per cut or per pass. Written documents could not possibly address all possible eventualities of workpiece size, shape, and material or machine type and capacity or dimensions, tolerances, and surface finish necessary to produce. Vendor inprocess control sheets or travelers are used to control the flow of material in the shop. While in process, machining is seldom, if ever, controlled by fixed documents the end results are carefully specified on the drawings.

**Question 19**

*In the design requirements for reactor shroud repair Specification No. M1B-1, Revision E0), GE stated that all parts have been designed to be removable. This design feature should be taken advantage of when planning inservice inspection of the core shroud repair components. The staff realize that the repair assemblies may be inspected by a combination of visual and ultrasonic examinations. However, the staff has some concerns regarding the reliability of such inspection to identify the potential degradation in the threaded joints and areas of crevices and stress concentration, which have limited access for inspection. Please provide a discussion and/or propose an alternative inspection such as disassembling the threaded joints for inspection to ensure that the areas mentioned above in the repair assemblies will be adequately inspected for early detection of potential degradation.*

**Response 19**

Pilgrim is committed to making appropriate inspections of the shroud stabilizers to ensure they maintain their functionality. A long range inspection program will be developed based on the recommendations of the BWRVIP and experience of the BWR fleet during the next year.

We have no information which would indicate the stabilizers are subject to stress or crevice corrosion under the design conditions. In addition to being fabricated of materials which are resistant to these types of attack (see response to Question #25), the tierods are

normally in lightly stressed (less than 30% of yield) conditions. They are only subject to high stresses under infrequently experienced conditions such as thermal upsets, seismic and LOCA events.

The location of the shroud stabilizer hardware, in the narrow annulus between the vessel wall and the core shroud and flanked on both sides by jet pump assemblies, is not the most convenient place to apply and manipulate remote-control underwater tools and devices. There is always a finite possibility that something could slip, get dropped, get bent, or some other mishap occur. In this case the possible advantage of being able to "detect potential degradation" at an earlier-than-normal stage in the lifetime of the assemblies by periodically taking them apart and putting them back together does not outweigh the multiplication of risks such operations would entail. There is also ALARA to consider.

Corrosion resistance played a major role in the selection and specification of the materials used. Not just 'corrosion resistance' in general terms, but specifically a proven history of corrosion resistance in BWR environments.

**Question 20**

*Plecse provide your basis for inspecting only 4 inches of each vertical weld intersecting at H-4 weld (ID and OD).*

**Response 20**

The cracking in the shroud vertical welds has been limited and where they have occurred, the crack lengths are small (less than 3 inches in all plants except one where one 15-inch crack was observed). This compares with instances where virtually continuous 360 degree circumferential cracks (approaching 600 inches) were found. The more extensive cracking in the horizontal welds could be due to the presence of end grain orientation, cold work due to machining, and the higher residual stresses due to the circumferential welds. On the other hand, the vertical welds have somewhat lower residual stresses because of the greater compliance of the vertical welds. The presence of fluence could also reduce the residual stresses further. Also, there is less likelihood of cold work when compared to the circumferential welds.

The vertical welds between the horizontal welds H2 and H5 are subjected to a significant neuron fluence level during reactor operation and, therefore, both the limit load and linear elastic-fracture mechanics (LEFM) approaches were used in calculating the allowable flaw lengths. The only loading that needs to be considered is reactor internal pressure differences. The faulted condition (pressure difference = 25.5 psi for above the core plate) was found to give the smallest allowable flaw sizes. The allowable axial flaw length by the limit load method was calculated to be 414 inches and 97 inches based on the LEFM approach. The allowable axial crack length of 97 inches is very close to the maximum length (101.13 inches) of any vertical weld in the in Pilgrim shroud. Therefore, inspecting 4 inches of the vertical weld will confirm the allowable flaw lengths are not exceeded.

Thus, given that the extent of the cracking has been minor and that the allowable flaw lengths are large, the recommended inspection requirements are less stringent than those for the horizontal welds. A 4-inch length at the intersection with a horizontal weld, was

judged to provide adequate indication if significant cracking exists at a vertical weld. If significant cracking is found in the vertical welds, the scope will be expanded.

**Question 21**

*Please provide details of your planned inservice inspection (location, extent, frequency, methodology and justification) of the installed core shroud repair components. Your planned inspection should consider the staff recommendation in Item 19.*

**Response 21**

It is GE recommendation that BECo should conduct a VT-3 inspection by camera of the installed shroud repair assembly during the current outage. This will ensure the repair is installed properly. Pilgrim plans to check the torque on the tie rod nuts after the first operating cycle. Inservice inspection for the repair assembly and/or components will be addressed through the ongoing work within the BWRVIP

**Question 22**

*Please identify the lubricants that would be used on the machined threads during installation. What are the controls of the content of chlorides, sulfides, halogens and other elements that are known to promote stress corrosion cracking in stainless steel and high nickel alloy?*

**Response 22**

The use of a Nickel-Graphite antiseize thread lubricant is specified during installation of threaded surface. The applicable specifications for this lubricant limit the following elements known to promote intergranular stress corrosion cracking of stainless steel and high-nickel alloys:

- The maximum allowable level of halogens, when both sulfur and nitrates are less than 1 ppm, is 450 ppm.
- The maximum allowable level of sulfur, when both halogens and nitrates are less than 1 ppm, is 630 ppm.
- The maximum allowable level of nitrates, when both total halogens and total sulfur are less than 1 ppm, is 820 ppm.
- Allowable combined levels of halogens, sulfur and nitrates are limited by the below formula.

$$\frac{ppm\_Halogens}{35.453} + \frac{ppm\_Sulfur}{48.096} + \frac{ppm\_Nitrates}{62.004} < 13.2$$

The specific lubricant was ordered in accordance with GENE specification D50YP5B from Fel-Pro Inc.

**Question 23**

*Please describe the methods and its accuracy's in monitoring the magnitude of the preload in the springs and tie rods to ensure there is no substantial relaxation of the preload. Please also discuss the safety consequences if the preload is completely relaxed.*

**Response 23**

There is no current plan to monitor preload in the stabilizer springs. It is planned to check the torque on the tie rod nuts after the first operating cycle.

For the design of the Inconel X-750 springs it was assumed that the end of life preload might be 5 percent less than the initial installation preload. The maximum preload stress in either of the X-750 stabilizer springs is less than 10,000 psi. Given this low stress and the fact that the end of life neutron fluence (>1 mev) is less than 2E19 nvt, the 5 percent allowance is sufficient, and there is no need to monitor preload.

Estimating preload loss in the 3.5 inch diameter XM-19 tie rods is complicated by the fact that its mechanical preload is a small part of its total preload; most of the tie rod preload results from differential thermal expansion when at operating temperature. The loss of preload has been estimated considering several mechanisms (change in the material stress strain curve between ambient and operating temperatures, thermal creep, and irradiation relaxation). A specified upset case thermal transient can temporarily increase the normal operating temperature difference between the tie rod and the shroud from 12°F to 131°F; the loss of preload determination considered 26 such upset condition transients. The expected loss of mechanical preload in the tie rod was determined to be less than ten percent of its original mechanical preload. It has been demonstrated by analysis that the remaining total preload at operating conditions is sufficient to prevent the shroud from lifting (see the response to question number 2).

The upper and lower springs are installed with a small radial preload such that they provide radial support for the shroud during cold shutdown. Preload deflection of the lower spring is provided by machining the vessel side contact blocks based on actual in-reactor measurements. Final preload deflection of the upper spring is set during installation and the adjusting hardware is locked with positive locking retainers. The specific value of the spring installation preload is not critical. During normal operation, the shroud and springs expand radially due to thermal growth slightly more than the RPV due to both thermal and pressure, which will provide some radial preload of the springs and assures that the springs provide linear support for the shroud during normal operation even if all of the initial installation preload is lost. Even with no mechanical preload, in the radial direction, the springs, which would be subjected to compression for all earthquakes, would still provide a linear restoring force and perform their structural function. The radial preload on the springs is not critical to plant safety during cold shutdown as the reactor is not critical. The stabilizer springs, with all mechanical preload lost, are still prevented by their weight and configuration from any significant motion, shifting or vibration during cold shutdown.

The stabilizer tie rods are installed with a small vertical mechanical preload, to assure that all assembly clearance is removed from the tie rod components, thus the thermal preload will be adequate to prevent crack separation during normal operation. Preload also assures that components are tight during refueling operations with the shroud head removed, and provides approximately 3600 pounds of axial load on the 3.5 inch diameter tie rods. If this initial mechanical preload were all lost, the thermal portion of tie rod preload during operation would still be adequate as shown in Supplement A to the shroud repair stress analysis.

The effect of reductions in the mechanical and thermal tie rod preload are analyzed in Supplement A to the Pilgrim Shroud Repair Hardware Stress Analysis Report. It is shown that the mechanical preload on the stabilizer tie rods may reduce to zero when the shroud head is installed, depending on the actual shroud stiffness (see Question 2). Under zero preload conditions, there will also be a gap, or net looseness, between the tie rod and the shroud. This gap is overcome by the differential thermal expansion between the shroud and tie rod during reactor warmup and normal power operation. The resulting thermal preload, even accounting for the net looseness, is sufficient to maintain the shroud in compression during normal operation when pressure uplift forces are applied from the core flow. There will be no separation of any cracked welds that may occur during normal operating conditions.

During cold shutdown conditions when the tie rod mechanical preload could be zero (H2 and H3 welds fully cracked), the weight of the shroud and shroud head is sufficient to maintain compression on any cracked circumferential welds. Core flow during cold shutdown will apply upward forces on the shroud head and core plate when either the reactor recirculation or RHR pumps are operating. The uplift forces are distributed differently during cold shutdown than during power operation because there is no steam flow out of the steam separators. The shroud head differential pressure is lower for a given core flow without steam generation, while the core plate differential is approximately the same as during power operation. Taking these factors into account, there will be no separation of cracked welds during cold shutdown for core flows below 78% of rated core flow. The limiting case is where separation would occur at the H7 weld below the core plate. This calculation is based on a reduced shroud head differential pressure of 5.0 PSI at 100% core flow (versus normal 7.5 PSI) and a normal 18.23 PSI core plate differential. At 78% of rated core flow at cold conditions the net downward compression on the H7 weld decreases to zero.

It is not expected that total recirculation flow during shutdown would be greater than 60 percent of rated; thus the loss of all mechanical tie rod preload would not be expected to create a shroud lift situation during cold shut down.

The analyses performed for flow induced vibration are sufficiently conservative as to still assure the absence of flow induced vibration if all mechanical preload is assumed to be lost. Calculations of tie rod natural frequencies were found to be relatively insensitive to tie rod mechanical preload.

**Question 24**

*Recently, IGSCC was observed in the welds (heat affected zones) of the top guide and core support plate in an overseas boiling-water reactor (BWR). Therefore, the staff recommends that the welds in the top guide and core support plate at Pilgrim should be inspected during the upcoming refueling outage to ensure there is no unacceptable degradation.*

**Response 24**

The GE SIL 588 (dated 17 February 1995) provides the generic recommended action for the welds (heat affected zones) on the top guide and core plate.

Recommendation 1 (SIL 588)

BWRs without top guides wedges--- perform visual inspection of the members which provide the load path between the alignment pins, the top guide and the shroud. This inspection should include examination of the welds for cracking (enhanced VT-1 with a 1 mil or 1/2 mil wire resolution) and verification that the pins are in placed (VT-3 exam).

The top guide design for Pilgrim does not contain the rim to ring weld that is referenced in SIL 588 (Reference Drawing 2426-3-3M1B). The Pilgrim top guide ring was machined from a solid piece of steel. The SIL recommendation, as it applies to the Pilgrim design, is being reevaluated.

Recommendation 4 (SIL 588)

All BWRs--- inspect the core plate bolts to assure they are in place. A VT-3 inspection of accessible core plate bolts should be made during the installation of the core plate wedges.

Recommendation 4 of the SIL 588 applies to all BWR core plates. Pilgrim uses 24 preloaded core plate clamps to hold the core plate down to the shroud, rather than the studs which are typical of most BWRs. The entire core plate clamp is above the core plate and thus accessible for inspection if adjacent fuel assemblies are removed. The shroud repair requires access to this area at the 45, 135, 225 and 315 degree azimuths. It is proposed that the core plate clamps which are incidentally accessible at these four azimuths (a minimum of four clamps) be examined (VT-3) during this outage. As a part of the shroud repair, eight core plate wedges are being added between the core plate and the shroud. These wedges provide additional lateral restraint to the core plate.

**Question 25**

*Please discuss the reasons that GE selects XM-19 material for the tie rods instead of austenitic 304 or 316 stainless steel (low carbon content). The 304 or 316 stainless steel has extensive service experience in the BWR environment. It should be noted that the acceptable yield strength of XM-19 material is limited to 90 ksi. Since there is limited service experience of XM-19 material in the BWR environment, the staff recommends that an accelerated stress corrosion testing of a mock-up simulating the XM-19 tie rod thread joint in a BWR environment should be performed to ensure there is no development of unexpected degradation.*

**Response 25**

XM-19 has experienced no known failures or other problems in approximately twenty years of BWR service. This is considered to be adequate confirmation that the material is acceptable for use.

XM-19 was extensively studied and tested in the mid-1970s. Results of these tests were published in Document NEDE-21653, of which the NRC received copies in 1977. These documents contain all of the test information.

Operating Experience With XM-19.

In the middle 1970's in the interest of improving the margin of control rod drive(CRD) performance, GE developed and implemented the use of XM-19 for piston and index tubes in place of Type 304 stainless steel. The logic was that as a low carbon, high chromium, mildly stabilized(Nb,V), austenitic alloy, XM-19 would offer a higher margin of resistance to intergranular stress corrosion cracking(IGSCC) in the nitrided condition than Type 304. Nitriding involves heating to ~1100 degrees F for several hours and results in furnace sensitization of 300 series stainless steels. As a side benefit, XM-19 has a significantly higher strength than Type 304 so equivalent components are stressed to a lower fraction of yield stress in service. Since the late 1970's all control rod drives manufactured by GE have contained XM-19 piston and index tubes. This includes all BWR-6s(more than 1500 drives) plus several other BWR-4/5 s under construction at the time as well as all replacement drives manufactured since. In total there are easily more than 2000 such control rod drives in service.

By the nature of the CRD design there are numerous crevices including threaded joints exposed to the reactor environment. On the average 10 to 20 per cent of the drives at a given plant are refurbished each outage. During this work the drives are disassembled giving ample opportunity for examination and detection of problems. To date no instances of intergranular attack or IGSCC of nitrided XM-19 have been reported.

**Question 26**

*If the credit for the fillet or any circumferential welds in the core shroud is taken in the design of the proposed repair to maintain the required preload, please discuss in detail and provide the justification regarding the measures you plan to take such as inspection to ensure the welds are, and remain, in the condition assumed in the analyses. Please also discuss the feasibility of measuring the preload during plant operation.*

**Response 26**

While calculating the vertical stiffness of the shroud for gap calculation, two different methods of crack modeling were used. The first method assumed a through wall crack across the bottom of the fillet and the ring is assumed to rotate about the toe of the fillet, as shown in Figure 26-1. This configuration reduces the moment arm compared to the second method. The second method assumed a through wall crack at the top of the fillet, as shown in Figure 26-2. Consequently, the ring cross section had a higher moment of inertia, which results in a slightly higher stiffness compared to the first method. These two methods reasonably bound the possible H2 and H3 weld crack configurations. Based on these results, for Pilgrim, the first method (with the lower stiffness and thus more conservative) was used for stiffness calculation in the analysis. The analysis showed that there was no separation under normal conditions. Since the analysis is based on a through-wall cracking across the bottom of the fillet, the H2, H3, H7 and H8 welds do not have to be intact to make the shroud repair functional. It is, therefore, concluded that no inspection of these welds is required to maintain the preload. For details on preload measurement, see the response to Question No. 23.

An additional analysis was performed at the request of BECo. In this method the top guide ring was assumed to rotate about the edge of the shell and a through-wall crack was assumed, with no fillet weld considered, as shown in Figure 26-3. Even though this

method is unduly conservative, there was no separation under normal conditions. Due to the extreme conservatism in this method, the results were not used for preload calculation.

Supplement A to the Pilgrim Stress Report demonstrates that the minimum stiffness required for the Pilgrim shroud to preclude separation during normal operation is $1.40 \times 10^6$ lbs/in. This is considerably below the calculated value of $5.2 \times 10^6$ which is the shroud stiffness with all welds cracked, taking advantage of the fillet welds at H2 and H3. If no consideration is given to the fillet welds, the minimum calculated shroud stiffness is $1.85 \times 10^6$ lbs/in.

The preload during normal plant operation is discussed in detail in the supplement to the stress report. The design of the shroud stabilizers utilizes differential thermal expansion of the shroud versus the stabilizers to produce the "thermal preload" that provides the tension in the tie rod during reactor operation. The mechanical preload that is applied at installation is intentionally small for the purposes of removing all clearances and putting tension on the tie rod. This tension is a nominal amount, producing only 0.007 inches of elongation on the tie rod. Applying this tension by simple torque measurement is considered adequate given the purpose of the mechanical preload component.

The thermal preload is not monitored, per se, however the application of tie rod preload by differential expansion is considered to be the optimum method for achieving uniformity of tension among the four tie rods. The differential expansions are calculated for normal operating conditions using the material coefficients of expansion and conservative values for the steady state temperatures. The upper bounding case for tie rod thermal expansion load is the thermal upset case which is analyzed in the stress report. Monitoring thermal preload under operating conditions is not necessary nor is it considered practicable or feasible.

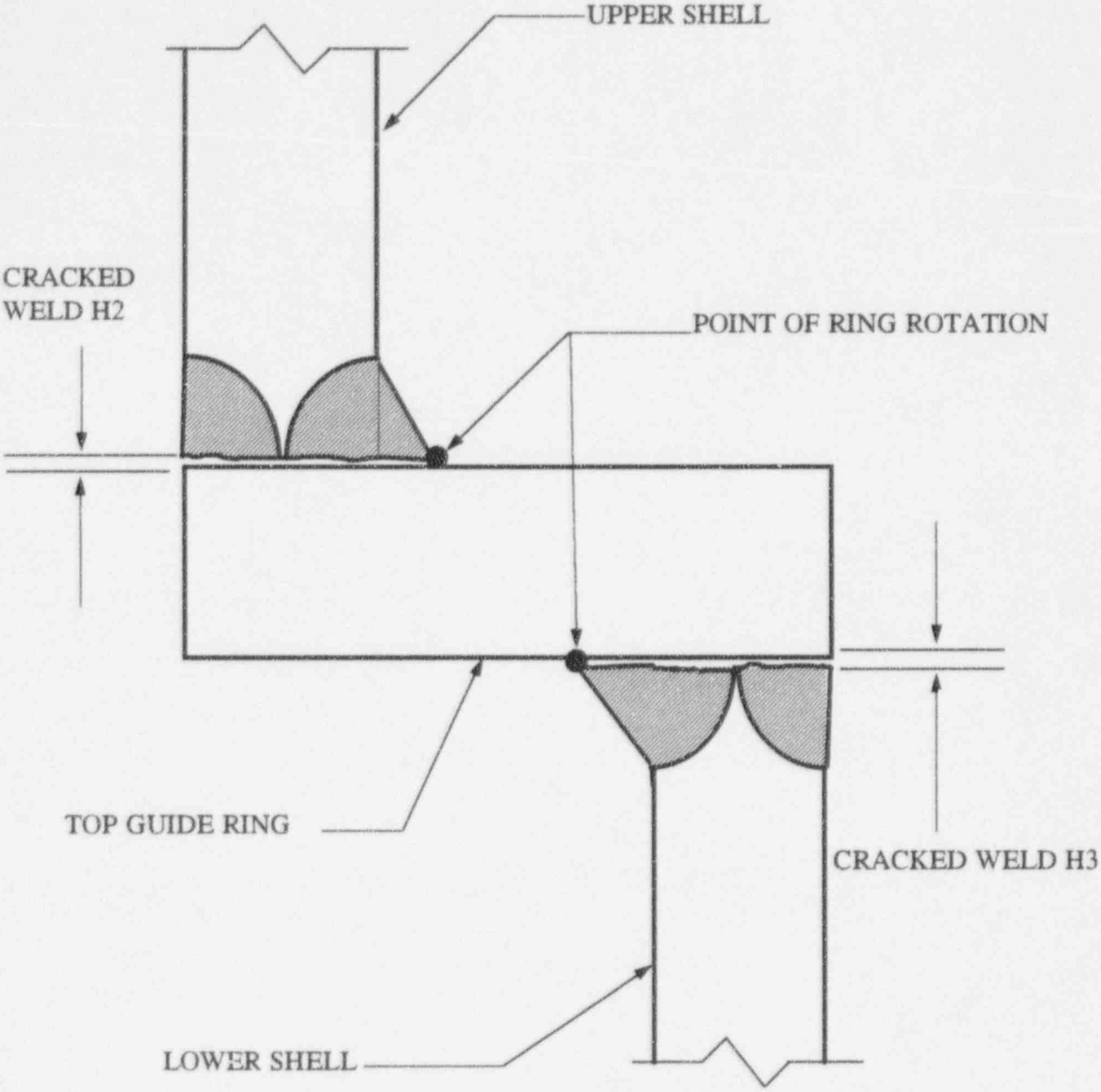


FIGURE 26-1
RING ROTATION ABOUT TOE OF FILLET
(NOT TO SCALE)

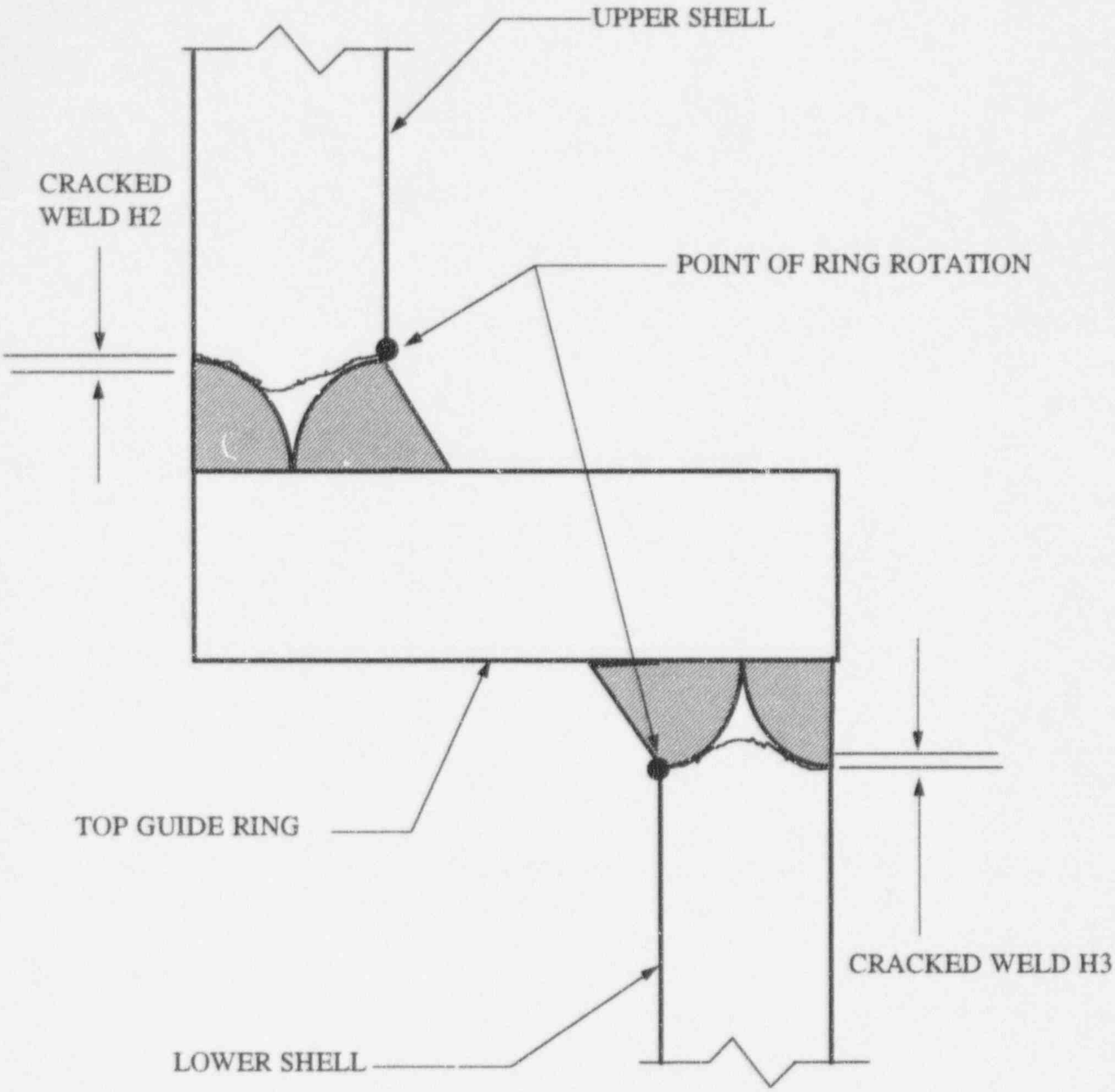


FIGURE 26-2
RING ROTATION ABOUT TOP OF FILLET
(NOT TO SCALE)

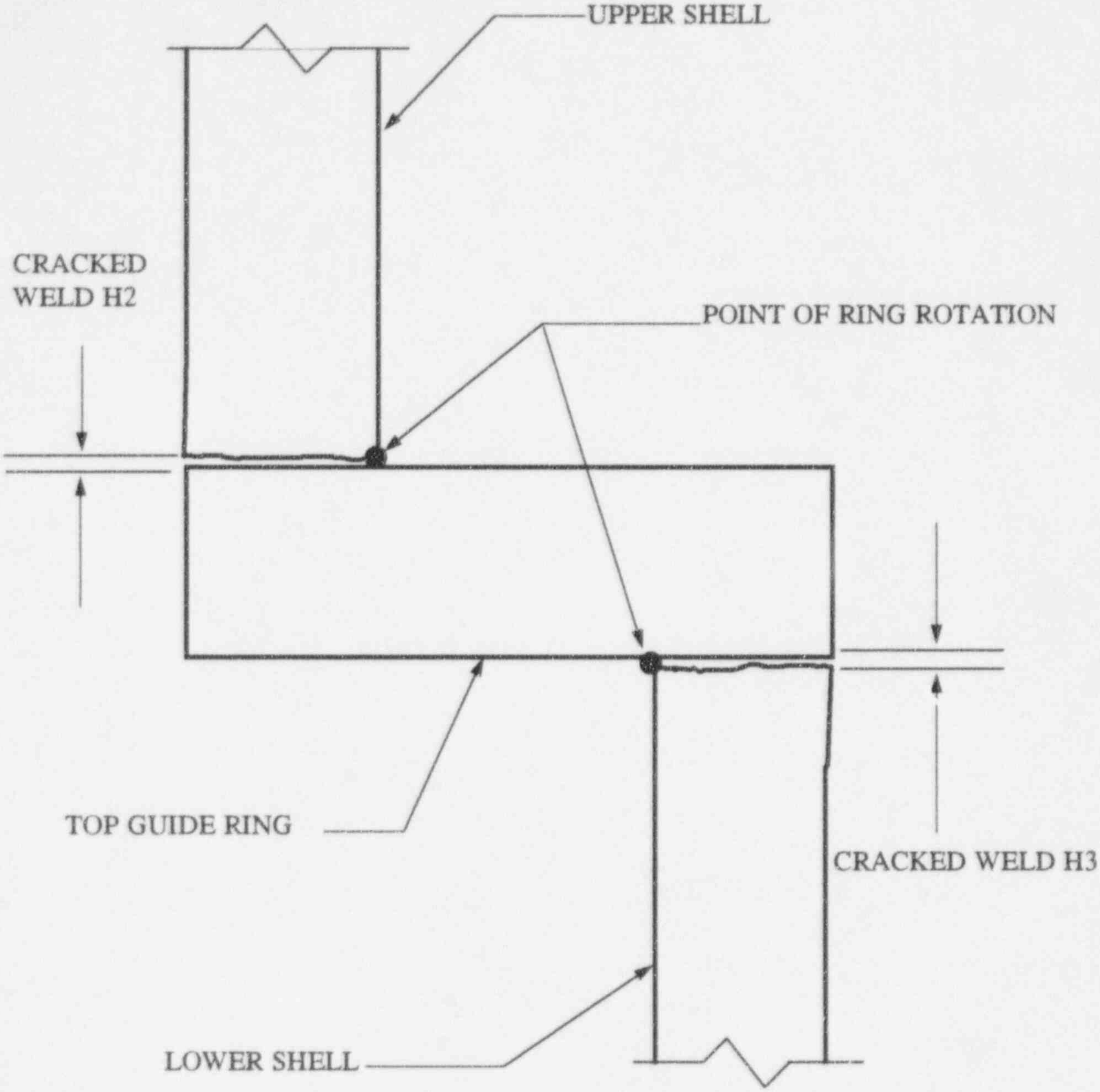


FIGURE 26-3
RING ROTATION ABOUT EDGE OF THE
SHELL

**Question 27**

*Please identify the ASME Code Material Specification Nos. that are specified for the procurement of the XM-19, 316 or 316L austenitic stainless steel and alloy X-750.*

**Response 27**

All of the materials are purchased to American Society for Testing Materials (ASTM) specifications: XM-19 = A182, A240, A336, A412, or A479. 316 or 316L = A182, A240, A336, or A479. X-750 = B637. The materials are identified in the GE Fabrication Specification 25A5604.

**Question 28**

*Please provide the heat treatment details such as time, temperature and cooling rate that are specified for the alloy X-750 components.*

**Response 28**

Heat treatment for the X-750 consists of 1975° ± 25° for 60 to 70 minutes, followed by forced-air cooling. Age hardening is done at 1300 ± 15° F for 20 to 21 hours, followed by air cooling.

**Question 29**

*Is solution annealed condition specified as the final material condition for all components made of XM-19 and 316 or 316L austenitic stainless steel materials?*

**Response 29**

Solution heat treatment is required for all 300-series and for XM-19 parts. This is done before machining and trimming. Threads on the XM-19 tierods are not annealed after machining. (See response to Question #15.)

**Question 30**

*Are Certified Material Test Report (CMTR) and heat treatment records available for all procured materials?*

**Response 30**

All materials procured for these assemblies are purchased with CMTRs. For the most part, and in keeping with the ASTM specifications, heat treatment information on CMTRs is limited to a statement that the material is "in the solution annealed condition", and the holding temperature is given. For the most part the mills do not include furnace charts or other specific details of the process to the suppliers from whom we buy the material. However, we require the following test, E50YP20, which assures that the material was adequately and properly heat treated.

**Question 31**

*In your response to Question 25, what is the material condition of XM-19 that were previously used in the BWR environment?*

**Response 31**

The XM-19 purchased for, and used to date, in BWRs is specified to be heated at 1950° to 2050° F for 15 to 20 minutes per inch of thickness minimum, then water quenched to below 800° F in 4 minutes or less.

Enclosure 3

Additional Information concerning Question 9, Question 21 and Question 24

Question 9

- Independent Parametric Analysis

  The licensing basis analysis was performed by General Electric Company using the original Reactor Building/RPV coupled mass model with soil springs at the building foundation interface, modified as necessary to represent the shroud repair hardware. Independent analyses were also performed by EQE Engineering Soil Structure Interaction (SSI) analysis. The three dimensional finite element model of the Reactor Building was recently developed in connection with ongoing work relating to Pilgrim's IPEEE (seismic) in response to Generic Letter No. 88-20, Supplement 4, and Unresolved Safety Issue A-46 in response to Generic Letter No. 87-02. In effect, these analyses provided a parametric evaluation of sorts by independently modeling the building, and the properties and behavior of the supporting soils.

  The coupled mass model was excited by a Housner SSE time history enveloping FSAR Figure 2.5-6, and by statistically independent synthetic time histories matching Regulatory Guide 1.60 and meeting SRP 3.7.1 requirements. The SSI modeling captures the shift of resonant frequency to a lower value than predicted by the licensing basis soil spring modeling. Lower bound, best estimate and upper bound soil properties were used for this analysis. Forces in the shroud repair springs and tie rods were calculated for the controlling shroud crack conditions defined by the General Electric Housner and R.G. 1.60 inputs, did not produce a more limiting case for design than the licensing basis analysis performed by General Electric.

Question 21

Pilgrim will perform a VT-3 inspection of the installed shroud repair assembly during the current outage. The inspection will ensure the repair was installed correctly. Pilgrim will check the torque on the tie rods after the first operating cycle. Future inspection plans will be based on the recommendations of the BWRVIP and the examination following the first operating cycle.

Question 24 (Recommendation 1 of SIL 588)

Inspection recommendations for the top guide are being re-evaluated.

Question 24 (Recommendation 4 of SIL 588)

The core shroud modification will install wedges to prevent core plate lateral movement. Therefore, the weld cracking described in SIL 588 will not be a major concern for PNPS. Accessible core plate clamps will be examined (VT-3) during wedge installation.