CEN-134-NP

C-E NSSS OWNERS' RESPONSE TO NUREG-0460, VOLUME 4 (March, 1980)

NUCLEAR POWER SYSTEMS

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POWER SYSTEMS

COMBUSTION ENGINEERING, INC.

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ABSTRACT

The purpose of this report is to answer questions and concerns stated by the NRC Staff, in Volume 4 of NUREG-0460, on the Anticipated Transients Without Scram Early Verification program. The major areas of concern expressed by the NRC Staff deal with Transient Analysis, Component Stress Evaluation, Fuel Behavior, Radiological Consequence and Moderator Temperature Coefficient.

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LIST OF ACRONYMS AND ABBREVIATIONS

AMSAC	-	ATWS Mitigating Systems Actuation Circuitry
ASME	-	Americar Society Of Mechanical Engineers
ATWS	-	Anticipated Transients Without Scram
BOP	-	Balance of Plant
CEA	-	Control Element Assembly
CLOF	-	Complete Loss of Main Feedwater
CVCS	-	Chemical and Volume Control System
EDM	-	Electric Discharge Machine
FSAR	-	Final Safety Analysis Report
FTC	-	Fuel Temperature Coefficient
GPM	-	Gallons Per Minute
HPSI	-	High Pressure Safety Injection
ITC	-	Isothermal Temperature Coefficient
KSI	-	Thousand Pounds per Square Inch
KTC	-	Kernal Temperature Coefficient
kw/ft	-	Kilowatts per Foot
LOCA	-	Loss of Coolant Accident
MTC	-	Moderator Temperature Coefficient
MWt	-	Power output in megawatts, thermal
NPSH	-	Net Positive Suction Head
NSSS	-	Nuclear Steam Supply System
PCI	-	Pellet Clad Interaction
PORV	-	Power Operated Relief Valve
PSIA	-	Pounds per Square Inch Absolute
PSIG	-	Pounds per Square Inch Gauge
PWR	-	Pressurized Water Reactor
RCP	-	Reactor Coolant Pump
RCS	-	Reactor Coolant System
RPS	-	Reactor Protection System
SPS	-	Supplementary Protection System

1.0 INTRODUCTION

In March, 1980, the NRC Staff released a Preliminary NUREG-0460 Volume 4 for comment (Reference 1-1). It contains the NRC Staff's proposed resolution of the ATWS unresolved safety issue (TAP A-9) in the form of requirements recommended to be imposed on licensees and applicants. The NRC Staff claims that their present recommendation is more extensive than the previous recommendations because the intended generic verification of the adequacy of the Alternative 3 modifications (Volume 3 of NUREG-0460) was not achieved. Reference 1-1 describes the proposed requirements and the recommended phased implementation, provides the staff's technical basis, and considers the values and impacts of the recommended solution. The appendices of Reference 1-1 describe the more recently submitted industry information and the staff's evaluation of that information, technical details, and some related risk estimates associated with the revised requirements.

The C-E NSSS owners are concerned that the ATWS issue is about to be resolved by the NRC without considering many of the relevant views on this issue. Therefore, on behalf of the NSSS owners, C-E has reviewed Reference 1-1 in its entirety. The purpose of this report is not to provide comment on the NRC's proposed requirements and their implementation nor on the value - impact and risk assessment arguments made by the NRC in these areas. Comments on these subjects have been addressed by the industry in other places. The purpose of this report is to answer the questions and concerns stated by the NRC Staff on the ATWS early verification program.

1.1 Report Structure

The NRC Staff concerns with the information and analyses presented by the industry in support of the adequacy of Alternative 3 are summarized in Section 1.3.1 of Reference 1-1 and discussed in Appendix A of Reference 1-1. The structure of this report lists the NRC concern from Reference 1-1 followed by the C-E Owners response. Several responses are expanded in the appendices to provide additional detail. Table 1-1 provides a cross reference to show where this report responds to the eleven items in Section 1.3.1 of Reference 1-1. Responses to the numerous Appendix A concerns of Reference 1-1 (which overlap Section 1.3.1 concerns) are addressed throughout this report.

The ATWS analyses presented and referenced in this report use the same plant classification as defined in Reference 1-2. As shown in Section 1 of Reference 1-2, the C-E plants can be categorized under three different classes: 2560 MWt, 3410 MWt, 3800 MWt. Pertinent parameters for these three different plant classes and the various C-E plants belonging to each of the classes are shown in Table 1-2 in Reference 1-2. The ATWS analyses were based upon typical parameter values shown in Table 1-3 in Reference 1-2 for each plant class, although these values do not necessarily represent a particular C-E plant. Use of these values in the ATWS analyses allows for the representation of many plants by the reference case analyses.

1.2 References for Section 1.0

1-1. Preliminary NUREG-0460, Volume 4 for Comment, "Anticipated Transients Without Scram for Light Water Reactors", Nuclear Regulatory Commission Staff Report, March 1980.

1-2. CENPD-263-P, "ATWS Early Verification", November, 1979.

TABLE 1-1

SECTION 1.3.1 OF REFERENCE 1-1 RESPONSE INDEX

Section 1.3.1 of Reference 1-1 Item	CEN-134 Response
1	2.6
2	2.6
3	2.7
4	2.3
5	3.6
6	3.1
7	Not applicable to C-E
8	5.0
9	2.7
10	2.6
11	3.6

2.0 TRANSIENT ANALYSIS EVALUATION

2.1 Code Verification

NRC Concerns:

Section 2.4.3.1, Page 37
"Furthermore, the Staff required verification of the B&W, C-E and Westinghouse system transient codes by appropriate startup tests..... Codes used to calculate peak pressures for ATWS analyses should be verified against high-pressure integral tests as discussed in Appendix B."

Appendix A, Section 2.1, Page A-21
"No verification against plant data has been submitted by C-E."

Response:

A CESEC verification program is being conducted using Arkansas Nuclear One Unit Two startup test results. Transient analyses results for a turbine trip, 4-pump loss of flow, full length CEA drop and part length CEA drop events are being used. Preliminary indications from the post test analyses show that CESEC compares favorably with the test results. The test data for the turbine trip and 4-pump loss of flow events indicated an initial pressure rise which CESEC also accurately predicted. While the predicted and measured pressure increases are not as large as those that may be experienced during an ATWS, the post test analyses demonstrated CESEC's applicability to pressurization transients. The CESEC verification results based on the Arkansas Nuclear One Unit Two startup test data will be submitted to the NRC by Arkansas Power and Light.

NRC Concern:

Appendix B, Page B-1
"The system transient or LOCA codes which are used in ATWS analyses must be verified against (1) appropriate test data, and/or (2) other codes which have been verified against test data. The appropriate test data can possibly be obtained from appropriately modified facilities of LOFT, semiscale and other facilities built for separate effects tests."

Response:

Certain small break LOCA analyses in References 2-1 and 2-2 have been referenced as being applicable to conservatively bound the long term response to some ATWS transients where significant voiding occurs. The LOCA code used for these analyses has been compared to the LOFT L3-1 integral tests results. Post test predictions using the small break LOCA code CEFLASH4A compared very well with the test results. The results of these comparisons were presented to the NRC and documented in Reference 2-3.

2.2 Limiting Case Determination

NRC Concern:

Appendix A, Section 2.2.1, Page A-25 Item (1)
"The use of the CLOF as the bounding ATWS may well be valid, but needs more justification. Given the range of plant configurations and the existence of other transients (e.g., zero power CEA withdrawal) which are close to the CLOF in peak pressure, we cannot accept the CLOF as always limiting based on what information we have."

Response:

In Reference 2-4 the statement, that the complete loss of main feedwater (CLOF) is the limiting at-power peak pressure transient, is still true. The peak RCS pressure results in Table 2-1 show that the relative ranking of the transients is consistent with that in the Reference 2-5 analyses with the exception of the zero power CEA withdrawal. The peak pressure for the zero power CEA withdrawal case for the 2560 MWt plant class was predicted to be slightly higher than the CLOF transient in the Reference 2-5 analyses. The zero power CEA withdrawal has been reanalyzed and the peak pressure calculated is 4160 psia which is less than the CLOF peak pressure. The stress analyses in Reference 2-4 and in this report show that the 4300 psia loadings allow the RCS components to remain intact following an ATWS and functionable to ensure the plant can be safely brought to cold shutdown.

The zero power CEA withdrawal was not a limiting case for the 3800 MWt plant class in the Reference 2-5 analyses and would not be a limiting peak pressure transient because of the more negative zero power MTC. A similar situation exists for the 3410 MWt plant class with its more negative zero power MTC relative to the 2560 MWt plant class.

Other concerns regarding containment pressure, fuel damage, and radiological releases are addressed in References 2-4 and 2-5 and Section 4.0 of this report. For all the transients analyzed, the results are well within the NRC guidelines for these areas of concern.

2.3 Sensitivity Studies

NRC Concern:

Appendix A, Section 2.2.1 Page A-25, Item (2)
"The sensitivity studies in the new report are limited to studies of the effect of pressurizer total relief area and of the moderator temperature coefficient of reactivity. This is not sufficient to demonstrate the applicability of the analyses to all plants in each class."

Response:

The ATWS Early Verification analysis in Reference 2-4 was performed for three generic classes of C-E plants. Except for the 3800 MWt class, the Reference Plant analyzed for each plant class is a composite conservative representation of the plants in each class. Table 1-1 in Reference 2-4 provides the values of the key plant parameters that impact the ATWS transients. The ATWS overpressurization consequences are directly impacted by the plant's ability to remove the energy transferred to the coolant. Thermal energy deposited in the coolant can be removed through heat transfer in the steam generators or carried through RCS leakage and flow with pressurizer relief and safety valves. The relative ability of the plants to mitigate the ATWS consequences can be determined by comparing the moderator temperature coefficient (MTC) and the ratios of (NSSS Power)/(RCS Relieving Flow) and (NSSS Power)/(RCS Volume). Table 2-2 is generated from information in Tables 1-1 and 1-2 from Reference 2-4 and summarizes these key parameters for the plants in each plant class.

The peak pressure calculated for the 2560 MWt class Reference Plant is conservative for all the plants in that class. The analysis for the Reference Plant assumed the limiting values for MTC and the ratios of (NSSS Power)/(Relieving Flow) and (NSSS Power)/(RCS Volume). For the 3410 MWt plant class, the Reference Plant analysis is also conservative for all the plants in that class. While the Pilgrim 2 (NSSS Power)/(RCS Volume) ratio is higher than the Reference Plant ratio, Pilgrim 2 has two power operated relief valves where the Reference Plant has none. The net result is that the peak pressure calculated for Pilgrim 2 would be less than that for the Reference Plant. The peak pressure calculated for the 3800 MWt Reference Plant is directly applicable to all the plants in that class except for the Perkins and Cherokee Units. These plants have smaller safety valves as evidenced by their higher (NSSS Power)/(Relieving Flow) ratio. The peak pressure calculated for these plants would be less than 4300 psia. The stress analyses documented in Section 3.0 of Reference 2-4 indicate that the 4300 psi loading is acceptable for maintaining primary system integrity and functionability of valves needed for long term shutdown following an ATWS event.

Other plant parameter differences such as Doppler feedback characteristics, steam generator secondary inventory and timing of auxiliary feedwater delivery have a negligible impact on the peak pressures calculated. During an ATWS, the time period over which significant power changes occur is several times the time constant of the fuel rods in either the C-E 14x14 or 16x16 fuel assemblies. Consequently, the response of both fuel types with respect to Doppler reactivity feedback will be approximately equal. The initial steam generator inventory does affect the time of the peak pressure but does not affect the magnitude of peak pressure calculated since the sharp pressure rise does not start until the inventory has been depleted enough to significantly reduce the heat transfer. The timing of auxiliary feedwater delivery, at the current capacities, does not significantly impact the peak pressure calculated. Calculations have shown that getting no auxiliary feedwater delivery prior to reaching the peak pressure only increases the peak pressure by 20 to 40 psia. Larger auxiliary feedwater capacity would reduce peak pressures by reducing the imbalance between heat generation and heat removal capabilities at the time that peak pressure is calculated to occur.

With the exception noted, the peak pressures calculated for each Reference Plant are conservative for the associated plant class. This is largely due to the fact that the analyses for each Reference Plant assumed the least negative MTC and, for most cases, the largest (NSSS Power)/(Relieving Flow) characteristics in that class. Other plant parameter differences, as discussed above, do not have as significant an impact on the pressure calculations.

NRC Concern:

Section 1.3.1, Page 7, Item (4)
"The impact of isolated PORVs on plant response to ATWS has not been adequately addressed."

Response:

In Reference 2-4, sensitivity studies were presented that showed the impact of pressurizer relief valve area on the peak primary system pressure reached during a loss of main feedwater ATWS. For the C-E designed nuclear steam supply systems, only the 2560 MWt plant class and Pilgrim 2 design includes power operated relief valves (PORVs). From Figure 2-36 in Reference 2-4, the peak primary system pressure calculated would be about 4300 psia for the case where the PORVs were isolated. The other system parameter trends and values would remain essentially the same as those presented in Figures 2-21 through 2-30 in Reference 2-4. The results of the stress analysis documented in Reference 2-4 indicate that a 4300 psia pressure loading would not impair the pressure retaining integrity of the primary components and valves necessary to achieve and maintain a safe shutdown condition.

2.4 Two Phase Fluid Discharge From Valves

NRC Concern:

Appendix A, Section 2.2.1, Page A-26, Item (3)
"More information on two-phase fluid discharge from the safety valves (and PORVs) is required. This information is expected to be available from the results of the upcoming EPRI test program."

Response:

The EPRI safety/relief valve test program will provide full scale test data at pressures well above the pressures for which data is now available. When the EPRI valve test data is available, much less extrapolation will be necessary to give reasonable assurance of the performance of safety and relief valves at the slightly higher pressures expected during some ATWS events.

2.5 Operator Action

NRC Concern:

Appendix A, Section 2.2.1, Page A-26, Item (4)
"The question of operator action has not been addressed in detail. The operators must be trained in ATWS mitigation and provided with written procedures which will enable them to unambiguously diagnose an ATWS, refrain from interfering with any mitigating system, and perform any supplementary investigating and monitoring actions that may be appropriate."

Response:

C-E is currently developing for the C-E Owners Group a two part effort to deal with procedures and operator training for RPS failures. The first phase is to develop appropriate procedure guidelines for use by the operator to handle a failure in the RPS under a wide variety of conditions. The ATWS scenarios are being considered in the guidelines as well as any RPS failure that may occur in the absence of any anticipated transient. The guidelines will provide information on ATWS symptoms to be used in diagnosing the event, operator actions to verify automatic mitigating system response and to assist in performing safety functions, and precautions concerning manual interference with the mitigating systems. These guidelines could then be incorporated into each plant's emergency procedures.

The second phase is to prepare a lesson plan for training operators in understanding and responding to an ATWS event and to an RPS failure in the absence of an anticipated transient. Detailed instructions on the event parameter trends and characterization, alarms, indications and equipment actuated will provide the operator with the knowledge to understand how an ATWS develops and thus the ability to unambiguously diagnose and respond to any ATWS event. The lesson plan is being based on the guidelines developed in the first phase and on the existing ATWS analyses (Reference 2-4 and Reference 2-5). The lesson plan will be used to train operators of C-E plants.

2.6 Reaching Cold Shutdown

NRC Concerns:

Appendix A, Section 2.2.1, Page A 26, Item (5)
"Experience has clearly shown that the means of bringing the plant to cold stable long-term post-ATWS shutdown must be well planned ahead of time. In the case of these C-E plants, the transients lead to voids in the primary system and tripped main coolant pumps. Analyses must be performed which demonstrate that the core can and will be adequately cooled, safely and reliably over an extended period of time, under these conditions. In this regard, the impact of lower shutoff head of some HPSI designs also needs to be carefully evaluated."

Appendix A, Section 2.1, Page A-21, A-22
"It is our position that the system transient code, CESEC-ATWS, is an inappropriate code to predict system behavior after the formation of the voids in the primary loop......Our judgement is that two-phase flow conditions with phase separation in the primary loop will exist.... The phase separation phenomenon would redistribute the voids in the core and primary loop. Void redistribution in the core would change the reactivity feedback and would consequently cause power variation during the long-term shutdown.....In order to predict these phenomena, an acceptable small-break LOCA code should be used. This small-break LOCA code should meet the requirements as appropriate to ATWS events."

Section 1.3.1, Page 7, Item (1)
"Not all significant anticipated transients were analyzed. The stuck-open power-operated relief valve (PORV) anticipated transient has not been correctly analyzed."

Section 1.3.1, Page 7, Item (2)
"Long-term shutdown has not been adequately addressed. In particular, the impact of voids in the primary system after the initial pressure peak has passed, the timing of the reactor coolant pump trip, and the plants with low high-pressure safety injection (HPSI) shutoff head have not been addressed. The PWR transient codes used in these analyses are unacceptable for situations where significant voids are calculated to be present in the primary."

Response:

These four NRC concerns are being addressed together since they all deal with the long term plant response in the presence of voids. Concerns with the stuck open PORV transient are addressed through discussion of the more limiting case of a stuck open safety valve. The impact of the timing of the reactor coolant pump trip is addressed below in the discussion on natural circulation.

The CESEC-ATWS computer code was developed primarily for calculating the system conditions through the point where the peak RCS pressure occurs and the reactor is brought subcritical. CESEC-ATWS is also appropriate for determining the potential for core uncovery. For the overpressurization ATWS transients, voiding does not occur until after the peak primary system pressure is reached. For the primary system depressurization transients, such as the inadvertent opening of PORVs, the core power is reduced due to negative moderator feedback and brought subcritical before significant voiding occurs. Comparisons with similar LOCA analyses have shown that the CESEC-ATWS code conservatively predicts the primary system mass transients relative to comparable LOCA analyses. The main reason for this is that CESEC-ATWS calculates a lower quality discharge through relief valves during two-phase flow conditions resulting in more mass being discharged. Table 2-3 lists the minimum primary system inventories calculated by CESEC-ATWS during the CLOF, CLOF with safety valve failure to close, and inadvertent opening of two PORVs.

The homogeneous flow model in the CESEC-ATWS code overpredicts the core average void fraction relative to more sophisticated LOCA analysis models. Recalculation of the core void distribution using the LOCA model shows a reduction in the negative moderator feedback and a possible return to criticality. Even though the reactor might return to criticality, no significant power would be generated. The feedback mechanism is that from a low void fraction, near saturation condition, any power generation that exceeds the very small primary system heat removal capability at that time, would heat up the RCS and increase the void fraction bringing the reactor subcritical again. In the longer term, the reactor is shutdown on boron because the operator is assumed to actuate charging and safety injection at ten minutes.

For all the cases listed above, the reactor is brought subcritical due to negative moderator feedback prior to reaching the minimum system inventory during the transient. The inadvertent opening of two PORVs and the loss of main feedwater with a stuck open safety valve result in system conditions less severe than those experienced during certain types of LOCAs. The LOCA calculations in References 2-1 and 2-2 show that substantially lower inventories than those listed in Table 2-3 can be tolerated and still keep the core covered, and remove decay heat, even without natural circulation. The LOCA analyses demonstrated that as long as the core remained covered, decay heat removal could be achieved. For the 2560 MWt plants, about 105,000 lbm of liquid is required to keep the core covered at a pressure of 1000 psia. Even at the somewhat higher post peak ATWS pressures, the inventories listed in Table 2-3 are sufficient to keep the core covered.

During ATWS transients where substantial primary system mass is discharged, the reactor coolant pumps (RCP) are calculated to cavitate due to low net positive suction head (NPSH). For the purpose of the analyses, the pumps are coasted down. The RCPs would actually be expected to operate and provide forced flow under a somewhat degraded condition until the operator tripped the pumps. As the pumps coast down, there is a transition from forced circulation to single and possible two-phase natural circulation. Descriptions for the LOCA analysis models for single and two-phase flow natural circulation are contained in References 2-6 and 2-7. As the primary pressure drops, saturation will occur and flashing will begin in the higher temperature and higher elevation sections of the primary system. For large primary system mass depletions, the primary side of the steam generators may drain, ending natural circulation. Core heat removal could then be accomplished via the reflux boiling mode, where boiling occurs in the core with condensation in the steam generators. The minimum primary system inventories listed in Table 2-3 are well above those required to keep the core covered in a reflux boiling mode.

During an inadvertent opening of a PORV ATWS and a CLOF with a stuck open relief valve ATWS, the primary system pressure will eventually decrease enough so that the high pressure safety injection flow exceeds the leakage rate. When this happens, the mass of water in the system will increase. The secondary side temperature and pressure will decrease as the steam generator inventory is reestablished by cooler auxiliary feedwater delivery and operator control of the atmospheric dump or bypass valves. As the primary system water mass increases, the steam voids will condense and result in the return to single phase natural circulation within 30 minutes.

ATWS calculations have been performed with the RCPs being tripped both before and after the peak RCS pressure is calculated to occur. The loss of offsite power ATWS analyzed in Reference 2-5 has the RCPs coasting down at time zero and no fuel damage occurs. With the ability of the system to remove core decay heat in either a natural circulation or a reflux boiling mode, the timing of tripping the RCPs does not make the consequences of the ATWS more severe. However, the tripping of the RCPs prior to the pressure peak does tend to reduce the pressure peak somewhat because it causes a more gradual reduction of the steam generator heat transfer coefficient. This gradual reduction in heat transfer coefficient causes the reactor coolant average temperature to be higher at the time of steam generator dryout which, in turn, causes the reactor power to be lower.

The ATWS analyses in References 2-4 and 2-5 considered the effects of the low shutoff head HPSI pumps with respect to long term shutdown. Analyses for all the plant classes in both reports assumed the HPSI pumps had a low shutoff head (1250-1400 psia). In all the analyses no HPSI flow of borated water was assumed until the RCS pressure went below the shutoff head of the pumps.

<u>NRC Concern</u>:

Section 1.3.1, Page 8, Item (10)
"If HPSI is actuated early (automatically or manually) while the primary system pressure is above the HPSI design pressure, its operability and integrity are questionable."

<u>Response</u>:

The HPSI subsystem operability and integrity are maintained should it be actuated while primary system pressure is above HPSI design pressure. The typical features previously reported in each plant's Final Safety Analysis Report are provided here for convenience. Two redundant and diverse check valves preclude fluid flow from the primary system to the HPSI subsystem through the injection paths. The check valves will be closed while the primary system pressure is above the HPSI design pressure maintaining system integrity. In addition, during the injection mode of operation, the HPSI pumps are provided with minimum flow protection to prevent damage resulting from operation against a closed discharge. The minimum flow circulation lines at the HPSI pump discharges are open to prevent shutoff head operation while primary system pressure is above the HPSI subsystem pressure. Operability of the HPSI subsystem is maintained. When the primary system pressure falls below the HPSI shut off pressure, the pumps deliver flow through the injection paths to the primary system.

<u>NRC Concern</u>:

Appendix A, Section 2.6.1, Page A-38, Item (3)
"The C-E reports do not discuss the effects of "typical" RHR leakage or the potential for damage to seals on ECCS equipment as a result of the ATWS system pressures and how these two pathways could contribute to the ATWS doses for Alternative 3 plants.

Response:

The Shutdown Cooling System and ECCS systems are isolated from the ATWS peak pressures. The HPSI system is protected by two redundant and diverse check valves as discussed in the above concern response. When the Shutdown Cooling System communicates with the reactor coolant pressure, the pressure has been reduced to operational limits. Since no fuel failures are predicted for ATWS events, the fluid activities will be within normal expected ranges which do not exceed 10CFR100 requirements. "Typical" Shutdown Cooling System leakage is serviced by the drain and collection systems described in each plant's Final Safety Analysis Report. Fuel failure is discussed further in Section 5 of this report.

2.7 Electrical, Preventive and Mitigative Systems

NRC Concerns:

Section 1.3.1, Page 7, Item (3)
"Combustion Engineering (C-E) information reveals that some instrument capability will be lost due to high primary pressure; this is likely to be the case for the other PWRs also. Ability of the instruments and equipment needed for safe shutdown to withstand the pressure peak only partially addressed by C-E and not addressed at all by Babcock & Wilcox (B&W) and Westinghouse (W)."

Section 2.3.1.2, Page 20, Item (1)
"For PWRs, until Alternative 4A modifications are implemented, the instruments exposed to primary system pressure must be capable of withstanding the Alternative 3A peak calculated pressures."

Appendix A, Section 2.2.3.1, Page A-27
"In Section 3.7, C-E has provided the results of their review of the integrity of instrumentation transmitters, which form part of Reactor Coolant Pressure Boundary (RCPB) at ATWS pressures. These results indicate that a majority of these transmitters are likely to lose their functional capabilities after ATWS pressure surges. It is also important to note that C-E's review did not include the assessment of instrumentation sensor lines and isolation valves, which are in the applicant's scope".

Appendix A, Section 2.2.3.2, Page A-27
"Functionability of critical instrumentation systems (sensors, sensing lines and associated isolation valves) during and subsequent to ATWS pressure conditions."

Appendix A, Section 2.3, Page A-35, Item (8)
"Instrumentation - Information presented is fairly detailed and indicates many "typical" instruments probably would need upgrading for Alternative 3 plants. It would appear that this equipment capability ultimately would have to be addressed on a plant specific basis by each Applicant or Licensee."

Response:

As stated in Reference 2-4, the review encompassed all the instruments necessary for proper operation of the applicable mitigation systems. The review was performed on that instrumentation which would be subject to the peak RCS pressure transient resulting from the hypothetical ATWS event. For the systems identified, points in the system were determined beyond which the pressure transient was not expected to propagate. These points were established at major RCS pressure boundaries (steam generators, heat exchangers, etc.) and various check and/or isolation valves which were closed or would close as the transient occurred. This resulted in a narrowing of the review to instruments directly attached to the RCS pressure boundary. Other instruments located in the containment which might be useful in diagnosing an ATWS or core conditions were also included as supplemental information, but not required as part of the mitigating systems.

The instrumentation pressure rating information presented in Tables 3-1, 3-2, and 3-3 of Reference 2-4 was based on instrument specifications, vendors published literature and vendor informal opinions. The conclusions in that report were so stated since the normal system design pressure ratings were $\sim$ 3110 psi. Evidence in the form of analyses or empirical test data showing that they will not fail for pressures higher than that value is not available. To date there is no evidence that gross failures will occur due to pressures up to 4300 psia. If actual testing were performed, it may very well show little if any degradation in performance after the pressure peak occurs.

It should also be noted that the increasing RCS pressure will be displayed, along with other abnormally changing primary plant parameters, to the control room operators. As the pressure transient progresses to its peak over the 1 to 2 minute interval, alarms will warn the operator prior to reaching the peak pressure. In the analysis, operator action was not assumed to occur for 10 minutes.

The resultant alarms and proper operator training will improve the operator's response to initiate short-term corrective actions which can terminate or reduce the severity of the transient (see discussion on procedures and operator training in Section 2.5). The instrumentation required for long-term mitigation and subsequent plant cooldown is expected to be available for both automatic and manual control actions.

NRC Concerns:

Appendix A, Section 2.2.3.3, Page A-27, A-28
Information Needed
"(1) Specific design information for the Supplementary Protection System (SPS) and AMSAC including the system design description, design criteria and bases, functional logic diagrams, schematic wiring diagrams, electrical power supplies and physical arrangement drawings."

"(2) Demonstration of independence and diversity between SPS/AMSAC and normal scram system."

"(3) Conformance discussion as to how SPS meets the requirements of IEEE Standard 279 and the AMSAC meets the criteria in Appendix C, Volume 3 of NUREG-0460 (Ref. 1)."

Section 1.3.1, Page 8, Item (9)
"Design information on preventive and mitigative systems has been inadequately addressed."

Response:

Table 2-4 lists systems involved in ATWS mitigation on C-E plants. Specific design information for the existing mitigation systems including their instrumentation and controls have been previously submitted as part of each individual Applicant's Safety Analysis Reports. In addition, Section 5 in Reference 2-4 provided system descriptions, design parameters and bases of systems used for ATWS mitigation. That section provided an in-depth discussion of the function of each individual system in relation to the other NSSS systems. Although not all of these systems are classified as safety grade systems, the instrumentation and controls design currently meets the intent of NUREG-0460, Volume 3, Appendix C. Modification to the mitigating systems would not significantly improve their response to ATWS events. Some of the ATWS mitigating systems are designed as safety grade systems whereas others are designed to the same high quality standards of control systems. Upgrading all mitigating systems to safety grade systems is expected to have only a very small effect on improvement of the unreliability. The control grade mitigating systems are in general compliance with the criteria in Appendix C of NUREG-0460, Volume 3. Thus adequate information has already been supplied for the systems needed to adequately mitigate an ATWS.

2.8 REFERENCES for Section 2.0

2-1 CEN-114-P, Amendment 1-P, "Review of Small Break Transients in Combustion Engineering Nuclear Steam Supply Systems," July 1979.

2-2 CEN-115-P, "Response to NRC IE Bulletin 79-06C Items 2 and 3 for Combustion Engineering Nuclear Steam Supply Systems", August 1979.

2-3 Letter from George Liebler (C-E NSSS Owners Group) to D. F. Ross (NRC), Subject: LOFT L3-1 Test Analyses, February 28, 1980.

2-4 CENPD-263-P, "ATWS Early Verification", November 1979.

2-5 CENPD-158, Revision 1, ATWS Analyses, May 1976.

2-6 CENPD-137P, "Calculative Methods for the C-E Small Break LOCA Evaluation Model," August, 1974.

2-7 CENPD-137, Supplement 1-P, "Calculative Methods for the C-E Small Break LOCA Evaluation Model," January, 1977.

TABLE 2-1

LIMITING CASE DETERMINATION

Event	3800 MWt Class Peak RCS Pressure (psia)	2560 MWt Class Peak RCS Pressure (psia)
Loss of Main Feedwater	3800	4220
Loss of Load	3510	4020
Zero Power CEA Withdrawal	3530	4160
Partial Loss of Coolant Flow	3290	Not limiting
Partial Loss of Feedwater	Not limiting	4120

TABLE 2-2

PLANT CLASSIFICATION

2560 MWt Class	NSSS Power / Relieving Flow (MWt-hr/10^3 lbm)	MTC ($10^{-4}\Delta\rho$/°F)	NSSS Power / RCS Volume (MWt/Ft^3)
Ft. Calhoun	2.41	-0.4	0.221
Maine Yankee	2.93	-0.2	0.239
Palisades	2.12	-0.2	0.205
Calvert Cliffs 1-2	3.01/2.84	-0.2	0.244
Millstone 2	3.02	-0.2	0.244
St. Lucie 1-2	2.89/2.83	-0.2	0.244
Reference Plant	3.02	-0.2	0.244
3410 MWt Class			
ANO-2	3.36	-0.63	0.283
SONGS	3.38	-0.63	0.289
Forked River	3.38	-0.63	0.284
Waterford	3.69	-0.63	0.289
Pilgrim 2	2.61	-0.63	0.297
Reference Plant	3.69	-0.63	0.289
3800 MWt Class			
Perkins (1-3) and Cherokee (1-3)	2.29	-0.68	0.275
ANPP (1-3)	1.88	-0.68	0.275
WPPSS 3 & 5	1.88	-0.68	0.275
Yellow Creek 1 & 2	1.88	-0.68	0.275
NYSE & G 1 & 2	1.88	-0.68	0.275
Reference Plant	1.88	-0.68	0.275

TABLE 2-3

MINIMUM EVENT RCS INVENTORY

Transient	Reference Plant Class (MWt)	Initial RCS Inventory (lbm)	Min. RCS Inventory (lbm)	Reference
2 PORVs Inadvertently Opened	2560	471,370	427,500	2-5
Complete Loss of Main Feedwater (CLOF)	2560	471,370	319,000	2-4
CLOF With Safety Valve Failure to Close	2560	471,370	336,000*	2-5
CLOF	3410	509,740	365,200	2-4
CLOF	3800	550,070	405,600	2-4
CLOF With Safety Valve Failure to Close	3800	550,070	365,000	2-5

*Analysis was done assuming an MTC = $-0.6 \times 10^{-4} \Delta\rho/°F$. Minimum inventory would be expected to decrease about 30,000 lbm (336,000 to 306,000 lbs) assuming an MTC = $-0.2 \times 10^{-4} \Delta\rho/°F$ as in Reference 2-4.

TABLE 2-4

SYSTEMS INVOLVED IN ATWS MITIGATION

Auxiliary Feedwater System.
Pressurizer Safety Valves and PORV.
Safety Injection System, HPSI Mode.
Charging System
Condensate System.
Main Steam Isolation Valves.
Steam Generator Safety Valves.
Atmospheric Dump Valves.
Shutdown Cooling System.

3.0 COMPONENT STRESS EVALUATION

3.1 Components Analyzed

NRC Concerns:

Section 1.3.1, Page 7, Item (6)
"No stress evaluation has been provided for balance-of-plant components."

Appendix A, Section 2.3, Page A-34, Item (1)
"No information has been provided for BOP supplied components. Additionally it is not clear which C-E supplied components have been evaluated versus those that have not. This should also address the effect of ATWS on steam generator tube plugging criteria consideration."

Appendix A, Section 2.3, Page A-29
"For a couple of these highly stressed components (i.e., reactor coolant pumps and pressurizer heater elements), component operability may be required for safe shutdown of the plant."

Appendix A, Section 2.6.1, Page A-37, Item (1)
"The section on steam generator tube leakage needs additional discussion and more data. The C-E report does not provide a table or figure for SG leakage vs. time nor does it provide sufficient backup data and information to determine whether the C-E approach and numerical values used in this area are acceptable."

Response:

C-E supplied components considered in this evaluation include both primary and auxiliary components. Table 3-1 lists the primary components which have been evaluated along with the method of analysis used. These components have been evaluated for each of the following plants:

1. St. Lucie 1
2. San Onofre
3. Palisades
4. Fort Calhoun
5. Maine Yankee
6. Calvert Cliffs
7. Millstone 2
8. Arkansas Unit 2
9. System 80 Plants

The method of evaluation for each of these primary components has been to review previously accepted stress reports (Component Catalogue 3-1 through 3-38) and to ratio maximum stresses up to ATWS levels. In addition to these evaluations, a detailed elastic-plastic finite element analysis has been performed on the surge line elbow. As explained previously in Reference 3-6, the surge line elbow has a stress intensity value which exceeds level C stress intensity limits, but are still well within level D limits. The surge line elbow is a typical example of this situation. A description of this analysis is given in Appendix A.

Table 3-2 lists the auxiliary components which have been evaluated along with the method of analysis used. These components have been evaluated for three representative plant classes:

1. St. Lucie 1 (2560 mwt)
2. San Onofre (3410 mwt)
3. Arizona (3800 mwt)

Each of the above plants are typical for the representative plant class. The method of evaluation for each of these components has been to review previously accepted stress reports (Component Catalogue 3-39 through 3-65) and to ratio maximum stresses up to ATWS levels. The ratioing method has been supplemented by hand calculations for the auxiliary piping systems. As stated previously, a detailed finite element analysis has been performed on one active valve and one pressurizer safety valve as further verification of the ratioing method. All auxiliary components satisfy Level C stress limits when subjected to the hypothetical ATWS pressure. The one exception identified previously in Reference 3-6 is the letdown piping where Level D stress limits are exceeded. In this pipe line, the letdown isolation valves will close and isolate this system once the primary pressure drops to below 2700 psi.

Because the BOP systems are designed to meet ASME Code standards and the BOP valves and piping are similar in construction to C-E supplied components, it is not expected that an evaluation of the BOP components would produce results more severe than results obtained for the C-E supplied components.

Steam Generator Tube Plugging Criteria

The effect of ATWS on steam generator tube plugging criteria has been evaluated. C-E has performed steam generator tube burst tests on wasted and virgin tubes. C-E report CENC-1256 (Reference 3-4) dated February 1976 documents the tests performed. In all cases, since the tube burst pressure was well in excess of postulated ATWS pressures, the steam generator tube are shown not to fail. ATWS loadings, therefore, are not in conflict with the steam generator tube plugging criteria. The test report abstract and tube burst table are given in Appendix D. The minimum room temperature burst pressure of 7000 psi corresponds to a burst pressure of 6150 psi at 600°F.

Operability of Reactor Coolant Pumps and Pressurizer Heater Elements

The highest stress levels in the Reactor Coolant Pumps occur in the diffuser vanes for the hypothetical ATWS loading. These stress levels exceed the ASME Code Level C limits, but are within the Level D limits. No detailed analysis has been performed to determine vane deformation or vane to casing clearance. As discussed previously in Section 2, reactor coolant pump operation is not required for safe shutdown of the plant.

The pressurizer heater elements are subjected to external pressure loadings. Based on ASME Code Paragraph F-1352, the Level D external pressure limit is 6500 psi (the value in Figure 3-2 of Reference 2-4 is incorrect) with an ovality of not more than 1%. This pressure limit is approximately 1.5 times the predicted ATWS peak pressure and is therefore considered acceptable. Since level D is not exceeded, buckling will not occur and therefore the pressurizer heaters are not expected to fail due to the ATWS peak pressure.

3.2 Types of Analyses Used

NRC Concern:

Appendix A, Section 2.3, Page A-34, Item (2)
"It is not clear in all cases exactly where finite element elastic or inelastic analysis has been applied. The information requested in the February 15, 1979 letter to vendors (Reference 2) is considered the minimum necessary to perform a review. For finite element inelastic analysis, information needs were further discussed above."

Response:

Finite element analysis has been applied to several primary and auxiliary system components.

A typical surge line elbow has been evaluated with an inelastic finite element analysis. The surge line elbow is a typical component which exceeds level C stress intensity limits, but is well within level D limits. Details of this analysis are presented in Appendix A.

The pressurizer safety valve disc and nozzle disc and nozzle for the Arkansas and San Onofre plants have also been evaluated with an inelastic finite element analysis. Details of this analysis are presented in Appendix B.

The Arizona Shutdown Cooling Isolation Gate Valve has been evaluated with an elastic finite element analysis. Details of this analysis are presented in Section 3.4 of this report.

The final area which was evaluated by finite element analysis is the reactor vessel closure region. This analysis is described in Section 3.3 of this report.

All other components which have been considered have been evaluated by scaling results of certified stress reports and/or by performing simple hand calculations to determine membrane stresses. In these cases, results are compared to ASME Code Level B or C limits.

3.3 Vessel Closure Analyses

NRC Concern:

Appendix A, Section 2.3, Page A-34, Item (3)
"Vessel Closure Analyses - This analysis is the foundation on which the entire Alternate 3 evaluation for ATWS is based. As such, it must be thoroughly understood and demonstrated to have been performed in a conservative, not a realistic manner. As a minimum, the concerns noted in the text above must be satisfied, including the documentation mentioned for finite element analyses in the February 15, 1979 letter (Reference 2). It is also fairly obvious that for such a critical application an independent confirmatory analysis would be invaluable input to reaching a final decision."

Appendix A, Section 2.3, Page A-25
"The description of these calculations occupies less than two pages in the report, and is primarily qualitative in nature. We would need a much more complete description of these calculations to accept Alternative 3 in these plants, including a consideration of the stability of the vessel head when raised in this manner, and an error analysis carried through to confidence limits on the relief flow rate."

Appendix A, Section 2.3, Page A-32, Item (1)
"Finite element inelastic analysis methods were used. The preceding discussion gives information requirements when this method of analysis is used."

Response:

The use of inelastic finite element techniques has been shown to be an effective method for considering real material behavior such as temperature-dependent material properties and elastic-plastic effects in regions of high stress concentration. The methods used here do not attempt to utilize any new or untested analysis techniques since the analysis procedures required in this study have been used extensively in the past and have been verified against known solutions. The results of this analysis are based on the MARC general purpose finite element program (Reference 3-2) and material property data generated in the C-E metallurgical laboratory.

A description of the MARC general purpose nonlinear finite element computer program is provided in Reference 3-3. The MARC program has been used at C-E since 1970 for the solution to nuclear safety-related problems. The program solutions have been verified by comparison of results with both classical problem solutions and other certified computer program solutions. This verification includes a wide range of elastic and elastic-plastic problem solutions for a great number of geometrically different models. Reactor vessel closure and an error analysis is discussed further in Appendix E.

NRC Concern:

Appendix A, Section 2.3, Page A-32, Item (2)
"It is the staff's understanding that two "representative" vessel closures were analyzed to "represent" at least three different vessel designs. Insufficient information is available to evaluate the validity of this approach."

Response:

Two inelastic finite element head closure models have been analyzed. One model was for the St. Lucie #1 reactor vessel closure and the other was for a System 80 closure. The St. Lucie #1 closure is identical to all pre-System 80 designs with the exception of Fort Calhoun and Arkansas. The System 80 closure model is identical to all System 80 designs.

The Fort Calhoun and Arkansas closure evaluations were performed according to the following equation.

NRC Concern:

Appendix A, Section 2.3, Page A-33, Item (3)
"It is not clear to the staff that tolerance on the amount of preload applied to the reactor vessel bolts has been adequately taken into account. This is a significant parameter which can greatly affect the system pressure at which closure leakage would begin, and of course could thus greatly affect the maximum system ATWS pressure."

Response:

The detailed reactor vessel closure analysis considers the tolerance on the amount of preload applied to the reactor vessel studs. The initial stud elongation tolerance is ± 0.002 inches for all C-E plants. This tolerance indicates a possible load variation of ± 64.0 KIPS per stud or ± 1800.0 psi in the stud tensile stress at initial bolt up conditions. The load tolerance detailed data is presented in Table 3-7, and the effect on seal leakage is discussed in Section 3.5 of Appendix E.

NRC Concern:

Appendix A, Section 2.3, Page A-33, Item (4)
"It does not appear that the analysis has taken into account possible deformations and movements of the closure head dome itself. The control rod drive penetration housings are installed into the vessel head dome with relatively small partial penetration welds which by ASME code rules are not to be exposed to any bending moment type loading. Assurance must be provided that at these high stress levels, closure head movement or deformation will not impose a severe enough moment loading on one or more of the control rod drive mechanism penetrations such that combined with the high pressure housing to dome weld failure would result."

Response:

Closure dome deformation predictions and effects on CEDM seal welds are discussed in Appendix E, Section 4.

NRC Concerns:

Appendix A, Section 2.3, Page A-33, Item (5)
"There is also a concern raised by the staff and an NRC consultant that with head lifting and leakage past both closure gaskets, that the head may "cycle", i.e., continue to alternately lift and reseat somewhat analogous to "chattering" of a safety valve. It would result in large dynamic loads being imposed on the closure bolts and also fluid leakage would be much lower than assumed resulting in higher than calculated maximum system pressures. Assurance would have to be provided that this would not occur."

Appendix A, Section 2.2.1, Page A-25
"In addition, we would need more detail on how tightly the vessel head reseats itself after passing a jet of coolant driven by more than 4,000 psi. The report gives no detail on this."

Response:

The O-ring leakage phenomenon is not analogous to Safety Valve cycling. During the hypothetical ATWS, the primary pressure increases up to the O-ring leakage level and continues to increase beyond that pressure level. There is no pressure fluctuation when the head lifts that results in a pressure decrease. A safety valve, by contrast, is mounted at the end of a pipe and may be subjected to a drop in inlet pressure when the valve pops open. This drop in inlet pressure causes the valve to close. As the inlet pressure then increases, the valve will be forced open again.

The system behavior for these cases is different in two important ways. First, the closure dome is located on a large reservoir while the valve is located on a piping run. Second, the ATWS pressure levels continue to increase after O-ring leakage occurs while the safety valve inlet may be subjected to alternating pressure levels. Alternate lifting and reseating of the closure dome is not predicted, because no local or gross pressure decreases occur. Head reseating is addressed in Section 3.6.

3.4 Structural Integrity and Operability of Active Valves

NRC Concern:

Appendix A, Section 2.3, Page A-34, Item (4)
"Structural Integrity and Operability of Active Valves - Technical justification must be provided for each size and design, not arbitrary extrapolations from a single analysis."

Response:

All active valves in the C-E Reactor Coolant Pressure Boundary scope of supply for the Arizona, San Onofre, and St. Lucie #1 plants have been evaluated for the hypothetical ATWS pressure loading of 4300 psi (Component Catalogue 3-39 through 3-65). These specific plants are typical for each representative plant class (2560 MWt, 3410 MWt, 3800 MWt). The Level C stress limits of the ASME Code are satisfied when these valves are subjected to the hypothetical ATWS pressures. Since there are no cases where membrane stresses exceed Level C, there is no permanent deformation of the valve assemblies. Operability of these active valves is therefore not impaired by an ATWS.

Tables 3-3, 3-4, and 3-5 give the specific tag numbers and methods of evaluation employed for Arizona, San Onofre, and St. Lucie 1 respectively. For both Arizona and San Onofre, the vendor calculated stresses have been multiplied by the ratio of ATWS hypothetical pressure to design pressure. For St. Lucie 1, valve sizes, design conditions and actual wall thickness measurements have been used in evaluations which have been performed with either the pressure-temperature rating method of the ASME Code or by hand calculated primary membrane stress analyses. The calculated stresses or measured wall thicknesses for all three plants satisfy the Level C limits of the ASME Code. This indicates that no permanent deformation results from ATWS loading.

In addition to these evaluations, a detailed elastic finite-element computer analysis has been performed with a model of an Arizona 16 x 12 x 16 Shutdown Cooling Isolation Gate Valve. The valve body, gate, and a section of attached piping are modeled as shown in Figure 3-1. All material data and dimensions are based on Reference 3-1. The model is asymmetric about the axis of the pipe. The pressure is applied assuming that the valve is closed. The axial pipe force is applied on the end of the pipe containing the pressure and no pressure is applied on the closed side.

The area of influence of the valve closure force is computed by simulating the valve closing force by a thermal expansion of the gate. The value of the closure force does not need to be evaluated precisely if the stresses produced by it are not at the same location as the high stress caused by pressure.

The MARC General Purpose Nonlinear Finite Element Program (Reference 3-2) was used for the elastic static analysis of the valve.

The elastic analysis of the valve model in Figure 3-1 was performed at 4300 psi internal pressure. The highest stress was 18,380.0 psi at the inner surface of the attached pipe as shown in Figure 3-2. This stress is lower than the ASME Section III code minimum yield stress of 18,500 psi for the valve body material (SA182F316) at 650°F.

The valve closure load is simulated as a thermal expansion of the gate. The expansion necessary to maintain a tight contact at the gate/body interface produces stresses less than 1000 psi beyond the zone indicated in Figure 3-3. The area of high stress due to pressure therefore is not at the same location as the stress due to valve closure. The sum of all stresses due to pressure and valve closure are less than the yield stress of the material.

The 16 x 12 x 16 inch shutdown cooling valve remains elastic when subjected to 4300 psi internal pressure when the valve is closed. The deformation of the valve is therefore very small and the operability and integrity of the valve are not impaired by a pressure of 4300 psi.

Similar results are predicted for other valves where the valve body is significantly thicker than the pipe to which it is attached.

3.5 Letdown Heat Exchanger Piping

NRC Concern:

Appendix A, Section 2.3, Page A-35, Item (5)
"Letdown Heat Exchanger Piping - The report indicates that this piping may fail. Documentation has not been provided indicating that the dynamic effects of this failure have been evaluated and shown to have no effect on safe shutdown."

Response:

CENPD-263 indicated that in a portion of the letdown piping system, downstream of the Regenerative Heat Exchanger, service Level D stress intensity limits will be exceeded under the specified loadings. Following an ATWS the required safety functions of plant boration and primary coolant inventory control are adequately accomplished with the charging portions of the Chemical and Volume Control System (CVCS). The letdown portion of the CVCS serves no safety related function for this or any other event. Therefore, any piping failure of the letdown heat exchanger will not prevent the attainment of a safe shutdown once the failure has been isolated from the Reactor Coolant System using the qualified letdown isolation valves, which will happen following the ATWS pressure peak.

3.6 Pressurizer Safety Valves and PORVs

NRC Concerns:

Appendix A, Section 2.3, Page A-35, Item (6)
"Pressurizer Safety Valves, Relief Valves (PORVs), and Associated Discharge Piping - The information provided on safety valve structural integrity is lacking in detail as per comments made above for components where finite element analysis is used. Information on Safety Valve operability is qualitative and probably somewhat speculative. Assurance of operability in the 3000-psi range will probably have to wait for results of EPRI Safety and Relief Valve Test Program as discussed in NUREG-0660 (Reference 10). No tests in the 4000-psi range are planned. C-E has not provided any information on structural integrity or operability of PORV's.

Additionally, no information has been provided on the structural integrity and functional capability of safety and relief valve discharge piping."

Section 1.3.1, Page 8, Item (11)
"The effect of pressures substantially above the 3400-3500 psi range considered in Volume 3 is not well understood. In particular, the integrity and performance of safety and relief valves has not been assured; the TMI-related industry testing program is not expected to encompass this extreme pressure range.

Response:

Pressurizer Safety Valve designs for the Arizona, San Onofre, and St. Lucie #1 plants have been evaluated for ATWS pressure loadings. Results of this evaluation demonstrate that Level C stress limits are satisfied for each of these valve designs for each reference plant class.

A detailed finite-element analysis with MARC (Reference 3-2) has been performed on the nozzle and disc of the Arizona and San Onofre pressurizer safety valve along with an ASME Code Section III evaluation of the inlet flange and bolting for ATWS pressure loadings.

The St. Lucie #1 pressurizer safety valve has been analyzed by the vendor. This evaluation (Component Catalogue 3-50) demonstrates that Level C stress limits are not exceeded during the hypothetical ATWS pressure loading.

An excerpt of the C-E detailed analysis is provided in Appendix B.

Power Operated Relief Valves have been evaluated by ratioing design report stress values up to ATWS pressures. In addition, hand calculated membrane stresses have been determined to be within Level B limits. The Reference 3-5 stress report applies to all C-E supplied PORVs. The results are given below:

	Stress at 4300 psi	Level B Limit
Body	11.9	17.9
Outlet Flange Bolts	29.	59
Outlet Flange	22.	26.85
Inlet Bolting	26.5	53.6
Inlet Flange	13.6	24.5
Disc - Valve Closed	18.74	31
Disc-Skirt - Valve Open	28.6	31

C-E is involved in the EPRI Safety and Relief Valve Test Program. This involvement includes design and analysis of the proposed test facility including the valve test stand and discharge piping. A comparison of test results with analytical predictions is included in the test program. This on-going involvement in the test program will lead to verification of analytical techniques which can be applied to commercial plant systems.

NRC Concern:

Section 1.3.1, Page 7, Item (5)
"The calculated peak pressure for operating C-E plants would exceed 4000 psi even with the vessel head lifting as calculated to relieve the primary pressure. Also, many components exceed service level "C" stress limit."

Response:

Several primary components, see Table 3-8, exceed the ASME Code Level C stress limit when subjected to the hypothetical ATWS peak pressure. In all cases, the calculated stress levels for these components are well within the Level D stress level. By satisfying the Level D criteria of the ASME code, the pressure retaining integrity of the component is insured. The major implication of exceeding Level C, then, is not one of structural integrity but one of deformation.

All components which satisfy Level C criteria are assumed to experience elastic deformation only. Once the ATWS pressure returns to normal levels, the component will return to its original geometry and operability will not have been affected. In those cases where Level C criteria are exceeded, a portion of the pressure induced deformation is permanent. For those components which function primarily as pressure boundaries, some permanent deformation is acceptable and will not interfere with the component's integrity. For components such as the Reactor Coolant Pumps, however, geometric stability is important to maintain clearances between moving parts. The operability of these components must be evaluated on a case by case basis in order to evaluate the acceptability of the inelastic deformation. Exceeding Level C criteria does not in itself indicate that the component is no longer operable.

3.7 Non-Active Valves

NRC Concern:

Appendix A, Section 2.3, Page A-35, Item (7)
"Non-Active Valves - The brief evaluation description provided appears to indicate that for these valves an extrapolation of capability was also made based on the one 16-inch active valve that was analyzed. Such approach may be technically feasible where operability is not a concern. However, the information in the report is too brief to provide the required technical justification for the validity of the extrapolation."

Response:

Non-active valves have been evaluated in the same manner as active valves (Component Catalogue 3-39 through 3-65). Table 3-6 gives a sample valve evaluation.

3.8 References for Section 3.0

3-1 Arizona SI 655, 656: 16/12/16 Inch Motor Operated Gate Valve: Borg Warner Report NSR 77850-2 Rev. D dated June 27, 1978.

3-2 MARC-CDC, "Non Linear Finite Element Analysis Program", Rev. H, 1976. Control Data Corporation, Minneapolis, Minnesota.

3-3 "Elastic-Plastic and Creep Analysis via the MARC Finite Element Computer Program" by D. J. Ayres. Presented at the Office of Naval Research International Symposium of Numerical and Computer Methods in Structural Mechanics: Urbana, Illinois; September 8-10, 1971.

3-4 CENC 1256, "Steam Generator Tube Tests" dated February, 1976.

3-5 Dresser 2-1/2 x 31533VX Electromatic Relief Valve Report SR-315-9, Rev. 1 dated August 11, 1978.

3-6 CENPD-263-P, "ATWS Early Verification", November, 1979.

3.9 Catalogue of Component Design Reports Evaluated for ATWS Loading

3-1 Omaha Public Power District Pressurizer

3-2 Omaha Public Power District Piping

3-3 Baltimore Gas and Electric Co. Reactor Vessel Units 1 & 2

3-4 Baltimore Gas and Electric Co. Steam Generator Units 1 & 2

3-5 Baltimore Gas and Electric Co. Pressurizer Units 1 & 2

3-6 Baltimore Gas and Electric Co. Piping Units 1 & 2

3-7 Maine Yankee Atomic Power Reactor Vessel

3-8 Maine Yankee Atomic Power Steam Generator

3-9 Maine Yankee Atomic Power Pressurizer

3-10 Maine Yankee Atomic Power Piping

3-11 Florida Power and Light Co. St. Lucie Plant Unit No. 1 Reactor Vessel

3-12 Florida Power and Light Co. St. Lucie Plant Unit No. 1 Steam Generator

3-13 Analytical Report for Florida Power and Light Co. St. Lucie Plant Unit No. 1 Pressurizer

3-14 Analytical Report for Florida Power and Light Co. St. Lucie Plant Unit No. 1 Piping

3-15 Northeast Utilities Service Co., Millstone Point Station, Unit No. 2 Reactor Vessel

3-16 Northeast Utilities Service Co., Millstone Point Station, Unit No. 2 Steam Generator

3-17 Northeast Utilities Service Co., Millstone Point Station, Unit No. 2 Pressurizer

3-18 Northeast Utilities Service Co., Millstone Point Station, Unit No. 2 Piping

3-19 Arkansas Nuclear One-Unit 2 Reactor Vessel

3-20 Arkansas Nuclear One-Unit 2 Steam Generator

3-21 Arkansas Nuclear One-Unit 2 Pressurizer

3-22 Arkansas Nuclear One-Unit 2 Piping

3-48 Control Valves (Arizona RC-100, CH515)

3-49 Dresser Dwg. 3NC-007, Pressurizer Safety Valve (Southern California)

3-50 Pressurizer Safety Valve (St. Lucie #1)

3-51 4 Inch Gage Trim Valve (Southern California)

3-52 1 Inch Globe Valve (Arizona)

3-53 14 Inch Swing Check Valve (Arizona SI217)

3-54 3/4 Inch Globe Valve (Arizona RC-211, 213)

3-55 As-built Dimensions for St. Lucie 1 Valves from the Following Vendors: Atwood and Morrill, Crosby, Dragon, Fisher Controls, Westinghouse, Velan

3-56 3/4 Inch Globe Valve (Arizona RC-752)

3-57 3 Inch Gate Valve (Arizona RC-240, 241, 242, 243)

3-58 1 1/2F 2 1/2 Relief Valve (Arizona CH199)

3-59 3 Inch Swing Check Valve (Arizona SI522, 532)

3-60 Pressurizer Safety Valve (Southern California)

3-61 1 Inch Globe Valve (Arizona)

3-62 2 Inch Globe Valve (Arizona CH524)

3-63 12 Inch Globe Valve (Arizona SI615, 625, 635, 645)

3-64 16/12/16 Inch 1512 Lb Gate Valve (Arizona SI 651, 652, 653, 654)

3-65 2 1/2 X 31533VX-30 Electromatic Relief Valves, Pilgrim, St. Lucie 1, Millstone, Calvert Cliffs

TABL' :-1

PRIMARY COMPONENTS ANALYZED

Reactor Coolant Pumps
Steam Generator Tubes
Primary Piping Including Surge Line
Surge Line Elbow*
Pressurizer Heater Elements
Reactor Vessel Shell
Pressurizer Shell

*Detailed analysis

TABLE 3-2

AUXILIARY COMPONENTS ANALYZED

Active Valves	See Section 3.4 and Tables 3-3, 3-4, 3-5
Pressurizer Safety Valves and PORV's	See Section 3.6
Non-Active Valves	See Section 3.7
Regenerative Heat Exchanger	
Piping Which May Be Subjected to ATWS Pressure	

TABLE 3-3

ARIZONA ACTIVE VALVES ANALYZED

Check Valves

RC-244
CH-431
CH-433
SI-217
SI-227
SI-237
SI-247

Gate Valves

SI-651
SI-652
SI-655*
SI-656*

Globe Valves

RC-100E
RC-100F
CH-515
CH-516

*Detailed Analysis

TABLE 3-4

SAN ONOFRE ACTIVE VALVES ANALYZED

Globe Valves

RC-100E

RC- JOF

TABLE 3-5

ST. LUCIE 1 ACTIVE VALVES ANALYZED

Check Valves

RCV-1248
RCV-1249
RCV-2431
RCV-2432
RCV-2433
RCV-3217
RCV-3227
RCV-3237
RCV-3247

Angle Valves

RC-1100E
RC-1100F
RC-2110P

Gate Valves

RCV-3480
RCV-3652

Globe Valves

RCV-2515
RCV-2516

TABLE 3-6

SAMPLE VALVE EVALUATION

12 Inch Motor Operated Globe Valve (1500 lbs)

Arizona SI 615, 625, 635, 645

Borg Warner Valve Report NSR79540 Rev. D, dated 12-8-79

Design pressure = 3600 psi

Minimum Hydrotest pressure valve closed: 110% P_d = 3960 psi

valve open: 125% P_d = 4500 psi

Calculated stress intensities include the effects of pressure, seismic, pipe reaction, and thermal loadings. Multiply calculated stress intensities by $\frac{4300}{3600} = 1.195$

	1.195 Calculated Value	Allowable Level B
Body, Main Run	26.8 KSI	27.3 KSI
Body, Neck	20.0 KSI	27.3 KSI
Body, Thread Relief	19.7 KSI	27.3 KSI

TABLE 3-7

LOAD TOLERANCE OF REACTOR VESSEL CLOSURE

HEAD STUDS

A. 2560 and 3410 MWt Plants

1. Tolerance of stud elongation = $\pm$ 0.002 inches
   Stud length is approximately 30 inches
   Strain variation = $\frac{\pm 0.002}{30} = \pm 67.0 \times 10^{-6}$ in/in

2. Stud Area = 35.4 in^2
   $E = 27 \times 10^6$ psi

3. Stress Variation
   $\sigma = \varepsilon E = 67 \times 27 = \pm 1800$ psi

4. Load Variation
   $P = \sigma A = \pm 1.8\ (35.4) = \pm 64$ KIPS

5. Installation Manual for Calvert Cliffs and St. Lucie 1 specifies stud loads of:
   Hydrotest: 1.619×10^6 lbs/stud
   Normal Operation: 1.295×10^6 lbs/stud

6. $P_{nominal}$ = 1,295,000 lbs/stud

   $P_{max.}$ = 1,359,000 lbs/stud

   $P_{min.}$ = 1,231,000 lbs/stud

B. 3800 MWt Plants

1. Tolerance on stud elongation = $\pm$ 0.002 inches

TABLE 3-7 (Continued)

B. <u>3800 MWt Plants</u> -- Continued

2. $L = 38.88$ inches

$A = 39.556 \text{ inch}^2$

$E = 29.7 \times 10^6$ psi

3. $\delta = \frac{PL}{AE}$

$$P = \frac{\delta AE}{L} = \frac{.002(39.556)\ (29.7\text{x}10^6)}{38.88}$$

$$= \pm\ 60{,}433 \text{ lbs/stud}$$

4. $P_{nominal}$ = 1,432,068 lbs/stud

P_{max} = 1,492,500 lbs/stud

P_{min} = 1,371,635 lbs/stud

TABLE 3-8

PRIMARY COMPONENTS EXCEEDING ASME CODE LEVEL C

STRESS LIMITS

Byron Jackson 35 X 35 X 43 DFSS Pump Case

SG Tube (ΔP)

12 Inch Surge Line Elbow

Pressurizer Heater Element (external)

42 Inch Hot Leg Elbow

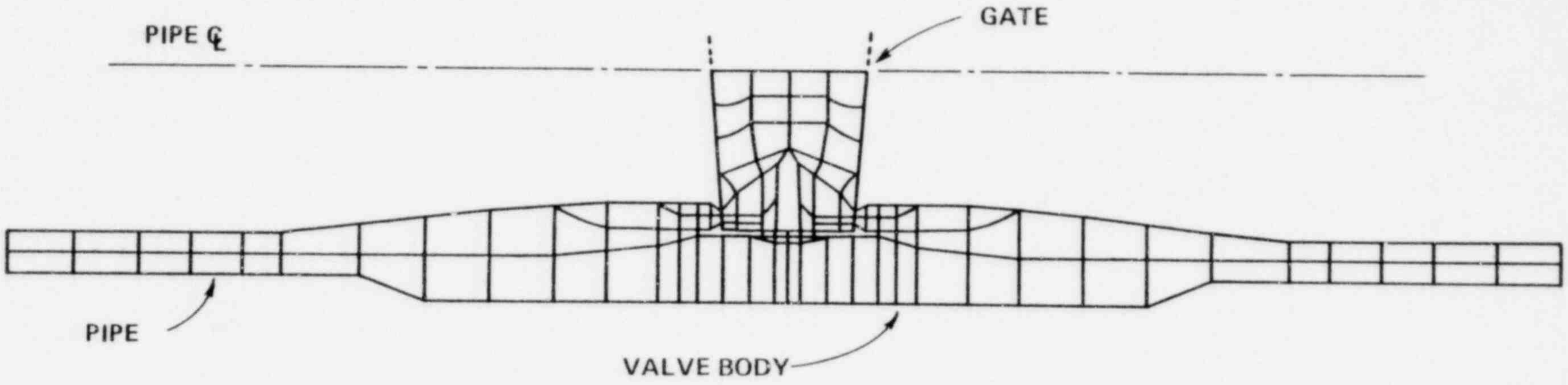
Byron Jackson 36 X 36 X 38 DFSS Pump Case

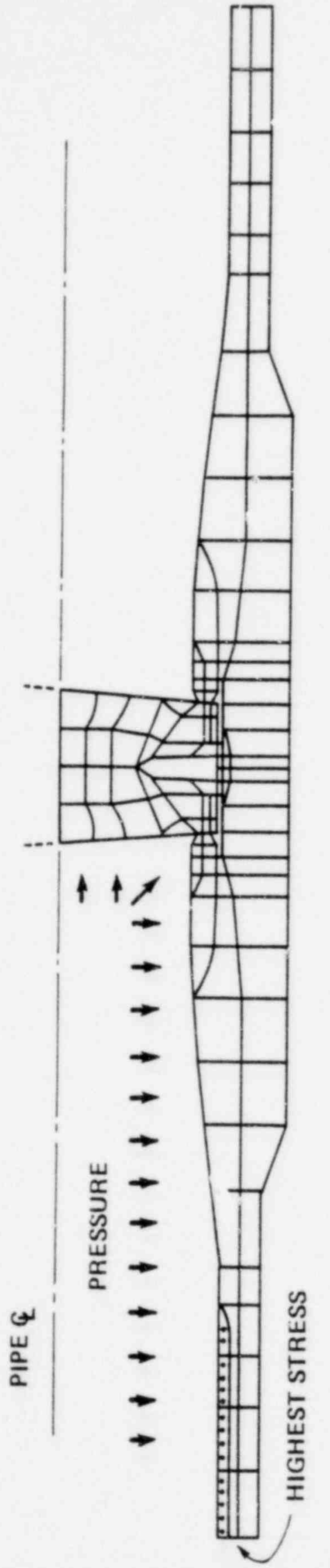
Reactor and Pressurizer Vessel Wall

CEN-134-P

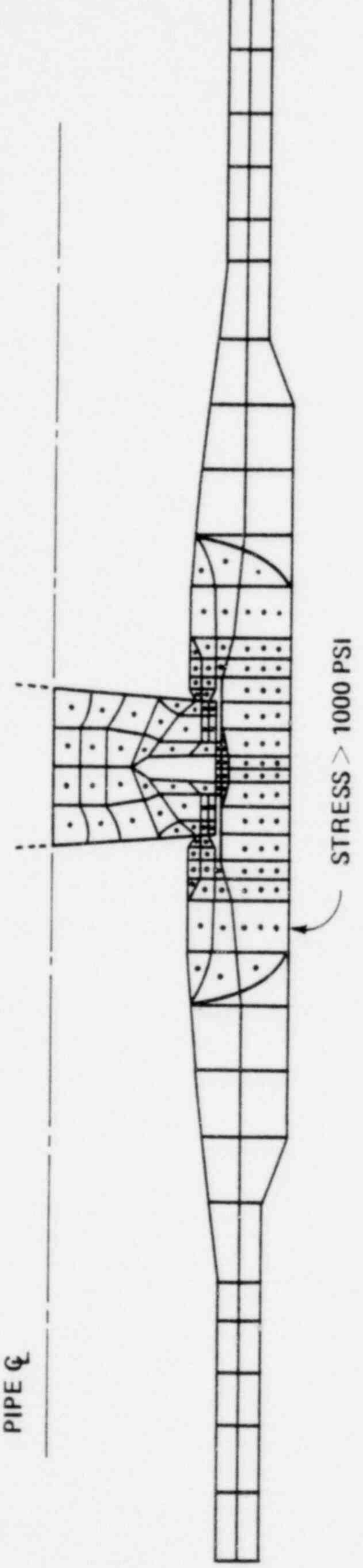
FINITE ELEMENT MODEL FOR 16 x 12 x 16" GATE VALVE

Figure 3-1





CEN-134-P	REGION OF HIGHEST STRESS DUE TO 4300 PSI PRESSURE ON CLOSED VALVE	Figure 3-2



CEN-134-P	REGION OF STRESS DUE TO GATE CLOSURE	Figure 3-3

4.0 FUEL BEHAVIOR

NRC Concern:

Appendix A, Section 2.5, Pages A-36, A-37
"Although the staff indicated in NUREG-0460 (Ref. 7) that C-E should provide an assessment of the likelihood for PCI failures resulting from ATWS events (such as rod withdrawal) that would involve reactivity insertions, PCI has not been addressed in any C-E ATWS analysis submitted to date. Such assessments may be made with the PROFIT PCI model so that the radiological dose consequences of those events can be estimated. To provide core-wide assessments of PCI failure probability, however, a census needs to be made of initial rod power distribution and burnup, since PROFIT provides a failure probability on a rod basis. That information needs to be correlated with the specific power history for a given transient. Regardless of whether C-E or NRC were to perform the calculations (NRC would have to acquire and collate more data), there would be a further delay, which would be inconsistent with the current ATWS resolution schedule. Therefore, as an interim measure, we propose that 10 percent of the rods should be assumed failed due to PCI resulting from power-increasing PWR ATWS."

Response:

The C-E Owners submitting this report can not agree that assessment with the PROFIT PCI model is appropriate, nor can we agree with the use of the proposed interim measure to assume ten percent of fuel rods fail due to PCI during all increasing power transients.

The current NRC Staff proposal would require assuming ten percent failed fuel rods for the zero power CEA withdrawal, full power CEA withdrawal, excess load, idle loop startup, and uncontrolled boron dilution events since these are the power increasing events (See CENPD-158, Rev. 1). The full power and zero power CEA withdrawal bound the increasing power transients with regard to ramp rate and maximum power density that is achieved for the longest period of time during the course of the ensuing ATWS event. The following paragraphs discuss the impact and inappropriateness of the NRC proposed interim solution for these two events.

The zero power CEA withdrawal event, as analyzed in CENPD-158, Rev. 1, and later calculations performed consistent with the results presented in CENPD-263-P, has been compared with experimental power ramp data obtained from the Petten test reactor (R. Holzer and H. Stehle, "Results and Analysis of KWU Power Ramp Investigation"; KTG-ENS-JRC Meeting on Ramping and Local Following Behavior of Reactor Fuel at Petten the Netherlands, Nov. 30-Dec. 1, 1978). During the postulated CEA withdrawal, the peak linear heat rate increases from near 0 kw/ft to a peak value of 9.6 kw/ft over a period of 380 seconds. (See Figure 2.1-8 of CENPD-158). The Petten test reactor data included a series of similar power ramp tests. Although presently C-E is not aware of any satisfactory failure criterion for PCI it is significant that four of the six test fuel rods survived similar power transients exceeding 14 kw/ft and the two failed fuel rods failed at peak linear heat rates exceeding 15 kw/ft. To C-E's knowledge, no fuel rod failures occurred during these or other ramp tests of similar fuel below a peak linear heat rate of 12 kw/ft. This data indicates that PCI is unlikely to occur during the zero power CEA withdrawal event.

The full power CEA withdrawal event also has been compared with experimental data for fuel exposed to similar power transients. The comparison indicates that some fuel may be in the range which makes it susceptible to PCI failures. However, even if it is postulated that some fuel fails, the radiological consequences would not be significant, since the maximum RCS pressure for this transient is less than the RCS design pressure. Subsequently, only a minimal amount of primary coolant will be relieved through the power operated relief valves or the pressurizer safety valves to the containment.

In summary, the cases examined, which bound the anticipated increasing power transients, indicate that there is no significant effect of PCI on radiological release from the primary system and the proposal to assume that ten percent of the fuel rods fail due to PCI during all power increasing PWR ATWS events is excessively conservative.

5.0 RADIOLOGICAL CONSEQUENCE EVALUATION

NRC Concern:

Section 1.3.1, Page 8, Item (8)
"Many questions remain on radiological evaluations if the containment structure is not isolated soon after the initiation of an ATWS event."

Appendix A, Section 2.6.2, Page A-38
"Given the information (Refs. 17, 18), the staff currently believes that the radiological consequences can be maintained less than the guidelines values of 10 CFR Part 100 if containment isolation is achieved rapidly .... If the isolation capability is provided, no more information is required on Alternative 3A plants.

Response:

As the NRC noted in Section 2.5 of NUREG-0460, Volume 4, the most-limiting ATWS DNB event considered is a partial loss of reactor coolant flow. It was concluded that "no fuel is expected to experience DNB and subsequently fail". The only other aspect of this NRC concern is associated with the radiological dose consequences pertaining to PCI failures as a result of ATWS events only.

The radiological releases from containment following an ATWS event are expected to be minimal for the two events discussed in the fuel behavior section (Section 4). For the full power CEA withdrawal event, where it can not be concluded that some PCI related failures will not occur, the radiological release will be negligible (even if failures are postulated) since only a minimal amount of primary coolant is relieved to containment. For the zero power CEA withdrawal event, where high system pressures may be reached and significant amounts of primary coolant may be introduced to containment, occurrence of fuel failures due to PCI is unlikely. The operator is instructed in the RPS Failure guidelines being developed to immediately initiate containment isolation upon indication of high radiation within the containment during an ATWS.

Since radiological releases from ATWS events are expected to be well below 10 CFR 100 guidelines, the existing methods of containment isolation are appropriate and additional containment isolation logic is unnecessary.

6.0 MODERATOR TEMPERATURE COEFFICIENT

NRC Concern:

Appendix A, Section 2.2, Page A-24
"The moderator temperature coefficient is now calculated to be unfavorably high in C-E plants (see Appendix C). Calculated peak pressures for these plants would be much higher even than the 4000-plus psi of Reference 18 without the headlifting relieving capacity."

Response:

A discussion on moderator temperature coefficients for ATWS analyses is provided in Appendix C.

7.0 SUMMARY AND CONCLUSIONS

This document responds to the questions and concerns stated by the NRC staff on the ATWS Early Verification program. The response of C-E NSSSs to ATWS transients is typified by the results presented for 2560, 3410 and 3800 MWt plant classes. From the ATWS analyses presented in this document and the analysis presented in CENPD-263-P, ATWS Early Verification, issued November 1979, the following results were obtained:

1. The most severe radiological release from any C-E NSSS during any ATWS incident is well within the limits of 10CFR100.

2. For all plant classes, the peak pressures calculated result in stresses that are below service level C for most RCS components. Components that exceed Level C are well below service level D stresses. The necessary RCS piping and valves remain functional following an ATWS so that the plant can be safely brought to a cold shutdown.

3. The long term coolable geometry of the fuel rods in any C-E NSSS is maintained following any ATWS since no event results in clad failure due to clad collapse, no fuel pins are calculated to approach incipient fuel melt, and no DNB condition is expected.

4. Containment integrity is guaranteed following any ATWS event since the maximum containment pressure resulting from any ATWS is approximately one third the typical design value of 50 psig (see CENPD-158, Revision 1). The unrealistic case of a safety valve failing in the open position results in a containment pressure still less than half the typical design value.

Appendix A

Surge Line Elbow Detailed Analyses

1.0 INTRODUCTION

The stress survey of the primary system components indicated that the 12 inch diameter surge line elbows were among the components which exceed ASME Level C (Emergency) limits at 4300 psi internal pressure. In order to determine the effect of the 4300 psi loading, a detailed analysis of the elbow, considering plastic deformation has been performed. The 12" elbow has a fairly simple geometry which is typical of many piping and pressure vessel components in the NSSS primary system. The analysis of the elbow, therefore, and the trends in the results are typical of many simple components in the primary system.

2.0 MODELS AND PROGRAMS

The finite element model of the 12 inch diameter surge line elbow and a section of the adjacent straight pipe is shown in Figure A-1 (Reference A-1). Symmetry about two planes is utilized to simplify the model and minimize computing time. The ends of the 45 degree section in Figure A-1 are constrained to remain 45 degrees from each other. This restraint simulates the effect of the attached piping.

The stress strain curve used for the analysis is shown in Figure A-2. The shape of the curve has been determined from C-E experimental data on typical specimens of type 316 stainless steel. The curve has been scaled to the ASME Section III Code minimum yield and ultimate stress values in order to assure conservative plastic behavior.

The MARC General Purpose Nonlinear Finite Element Program (Reference A-2) was used for the elastic plastic static analysis of the surge line elbow. The pressure is applied to the inside of the pipe and the equilibrating axial force is applied at the ends of the section. The pressure to cause first yield in the section is computed. Then the pressure is increased incrementally until it reaches 4300 psi.

3.0 RESULTS

The elastic analysis of the model in Figure A-1 was performed. The pressure to cause first yielding is found to be 3340 psi. The location of the yielded region is in the crotch (center of inside radius) as expected. Subsequent loading results in an increase of the size of the yielded region. At 4300 psi the maximum plastic strains are 0.0014 in/in. The extent of the plastic zone is shown in Figure A-3. The effective stress is only slightly above yield indicating a significant load redistribution away from the crotch area.

4.0 CONCLUSIONS

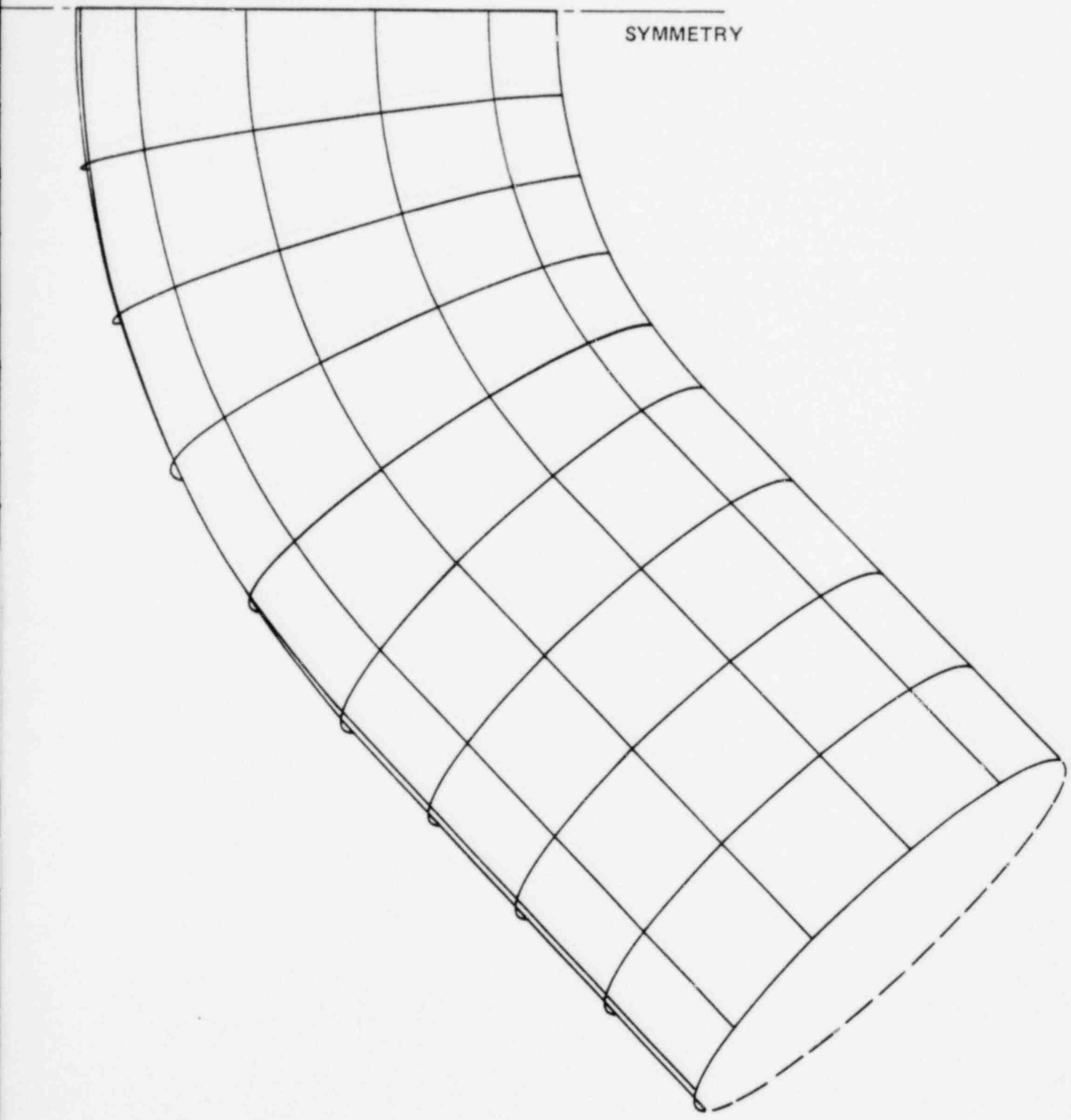
The deformation of the surge line elbow at 4300 psi internal pressure is small. The stresses redistribute away from the area of concentration that is examined in elastic ASME III Code type analysis. The functionability and integrity of the elbow are not impaired by a pressure of 4300 psi.

Similar results are expected for other components where the Primary stress is computed at an area of stress concentration.

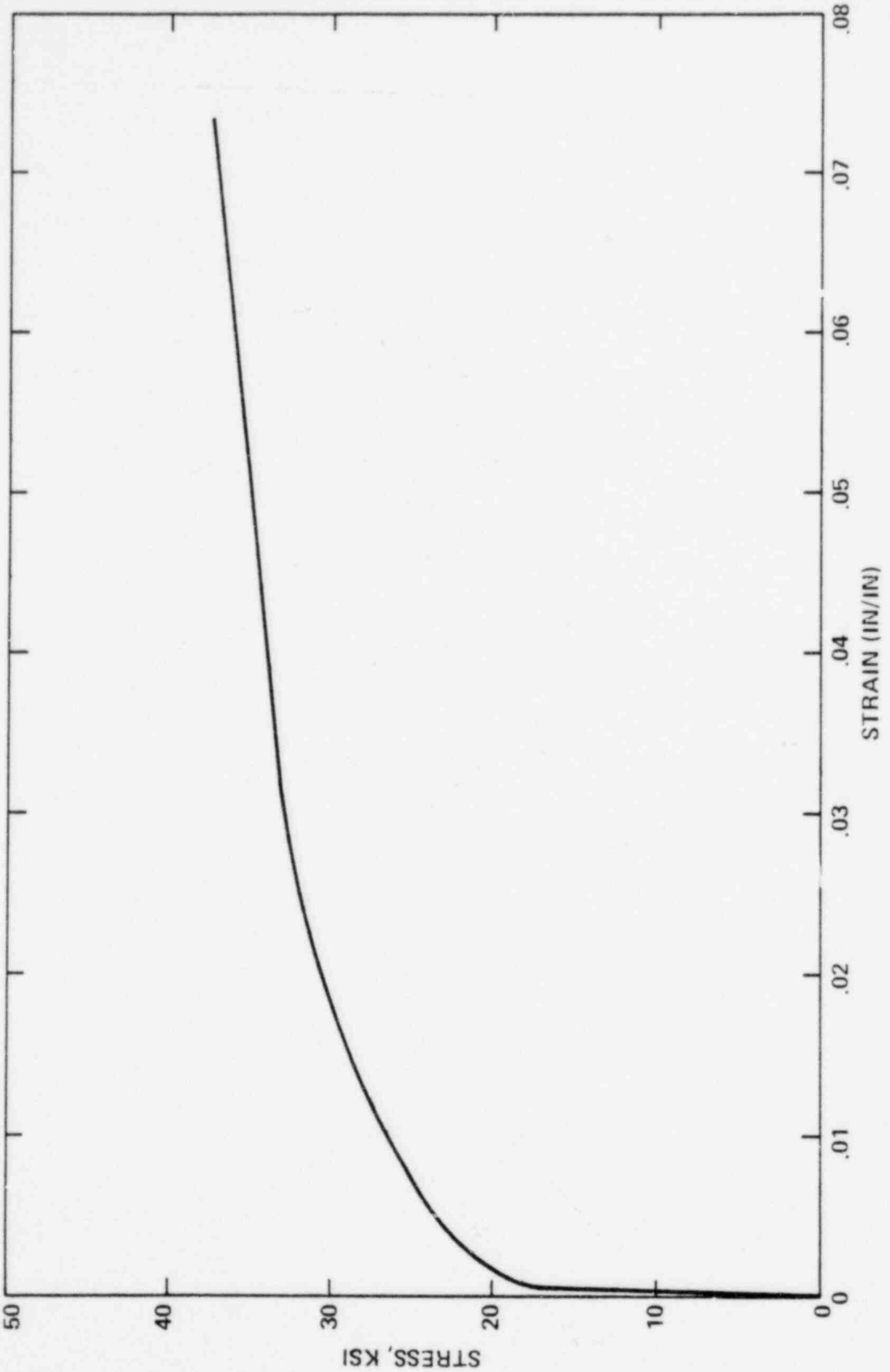
5.0 REFERENCES

A-1 C-E Drawing E-234-074 Piping Details: Florida Power & Light Co., St. Lucie I.

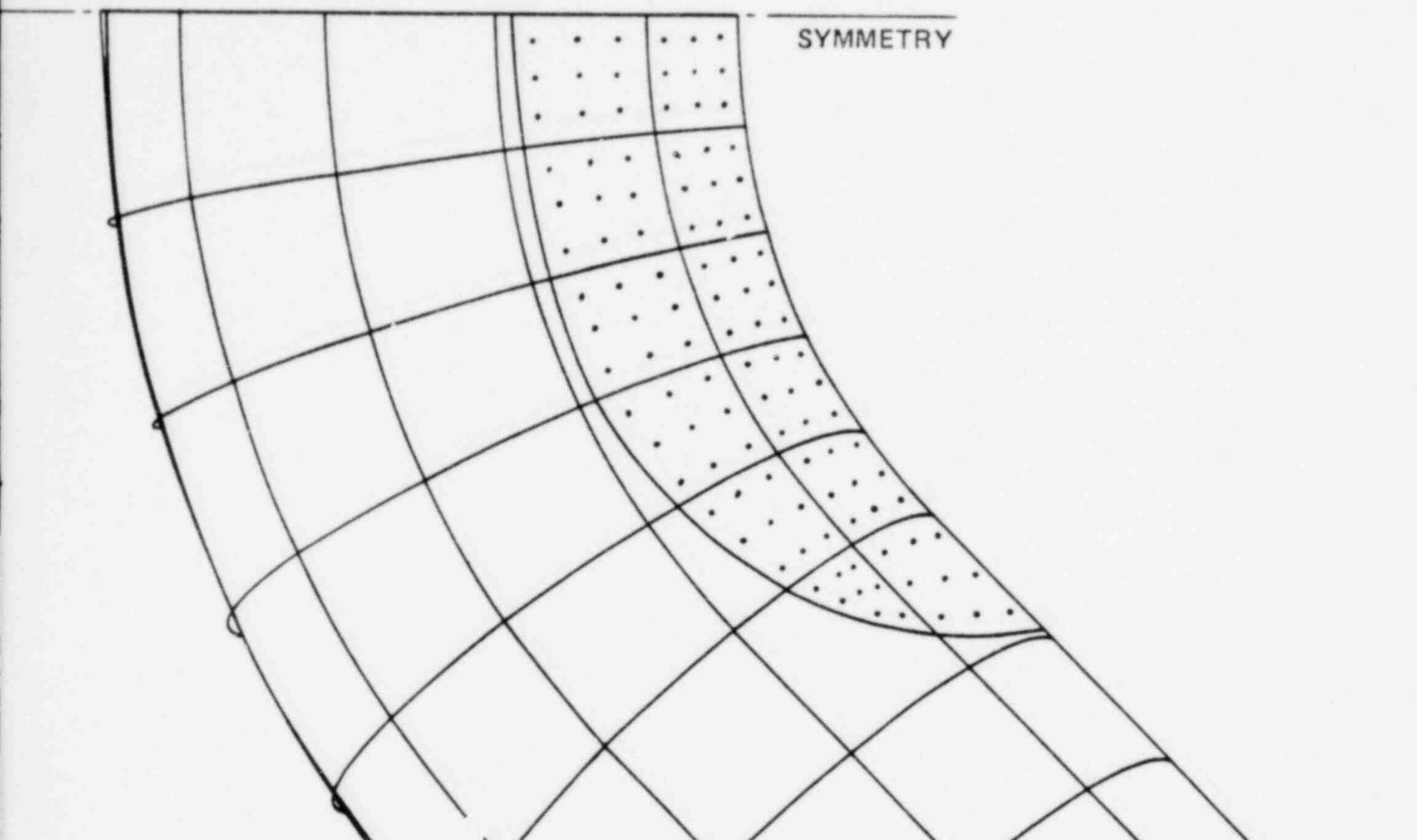
A-2 MARC-CDC, "Non-Linear Finite Element Analysis Program, Revision H, 1976, Control Data Corporation, Minneapolis, Minnesota.



CEN-134-P	FINITE ELEMENT MODEL OF SURGE LINE ELBOW	Figure A-1



CEN-134-P	STRESS STRAIN CURVE FOR SA351CF8M INDEXED TO ASME CODE MINIMUM PROPERTIES (SURGE LINE ELBOW ANALYSIS)	Figure A-2



CEN-134-P	SURGE LINE ELBOW REGION OF PLASTIC STRAINS AT 4300 PSI INTERNAL PRESSURE	Figure A-3

APPENDIX B

PRESSURIZER SAFETY VALVE DETAILED ANALYSIS

1.0 NOZZLE AND DISC

1.1 Purpose

The purpose of the analysis was to evaluate the operability and integrity of the pressurizer safety valve, for Arizona Public Service, Contract No. 14273; after it is exposed to ATWS pressure. The valve is evaluated to meet the requirements of ASME Boiler and Pressure Vessel Code, Section III, Subsection NB, 1974 edition. The valve is analyzed for spring load and pressure load as shown in Figure B-1.

1.2 Geometry

The geometry of disc and nozzle is as shown on Figures B-2 and B-3. Geometry is based on References B-4 and B-5.

Materials:

Disc.: ASTM A-637 GR.688 Type 2
S_y = 115000 psi
S_m = 51000 psi @ 700°F
E = 29.2 x 10^6 psi

Nozzle: SA = 182 F-347
S_y = 30000 psi
S_m = 20600.0 psi @ 700°F
E = 28.3 x 10^6 psi

Loadings:

(1) Spring Load = 15438 lbs
(2) Pressure Load = 2500 psi

1.3 Analysis

The model as shown in Figure B-4 is formulated using element type 28, an 8-node isoparametric axi-symmetric distorted quad ring of the MARC program. (Reference B-1). The disc is subdivided into 84 elements. The nozzle is subdivided into 74 elements. The disc and nozzle are the most important parts on which integrity and operability of the safety valve depend. Therefore, only the disc and nozzle of the valve are analyzed by finite element methods. The analysis is divided into three parts.

In the first part, the present load of the spring is applied on the top of the disc. The nozzle is supported at the lower end. The disc and nozzle models are tied at their contact surfaces.

In the second part, only the disc was analyzed for pressure loading. The disc was supported at the top surface. Pressures of 2500 psi and 3000 psi were applied to the disc; and the stresses were observed in the disc. The pressure loadings were scaled to the elastic limit of the disc material. The scaling was based on the results of 2500 psi and 3000 psi pressure effect.

In the third part, the whole disc was supported. The nozzle was supported at the lower end. The disc and nozzle were in contact initially; but they were not tied. A pressure load of 2500 psi was applied only on the nozzle. The pressure load was then scaled to the elastic limit of the nozzle material.

1.4 Results

The maximum stress in the disc, due to a point load equal to the spring preset force of 15,438 lbs. is 36.70 ksi which is well within the allowable value of 76.5 Ksi (1.5 sm) Reference B-2. This highest stress is produced in element No. 73 in the disc. The maximum stress in the nozzle is only 11.93 Ksi. The stress produced in the disc due to the pressure of 2500 psi is 49.80 Ksi and for the pressure of 3000 psi is 60.45 Ksi. These stresses are well within the "allowable" limit of 115.0 Ksi. Plastic strain is not experienced in the disc. From the results of 2500 psi and 3000 psi pressure loadings at the disc, the stresses in the disc are extrapolated for a 4300 psi pressure loading for integration point 4 in element 4. The maximum stress in the disc at 4300 psi pressure is 87.0 Ksi as shown in Figure B-5.

The maximum stress in the nozzle reached 30.0 Ksi when the pressure was 6350 psi. The yield stress limit (30.0 Ksi) was experienced in element No. 111. The nozzle remains in the elastic state at 4300 psi pressure loading. The finite element model and stress contour plots are shown in Figures B-6, B-7, and B-8.

1.5 Conclusion

The model used, the technique employed and the loading applied indicate that the disc and nozzle should remain elastic after the valve is exposed to ATWS pressure. The stresses produced in the disc and nozzle at 4300 psi pressure are only 87.0 Ksi and 20.0 Ksi respectively (by scaling). This is within the elastic limit of the materials. The maximum stresses produced show that the integrity of the valve is not changed by ATWS pressure. The valve will remain operable after exposure to ATWS pressure. The valve stem and spring are protected from excessive loads by the bellows disc nut which limits disc uplift as shown in Figure B-9.

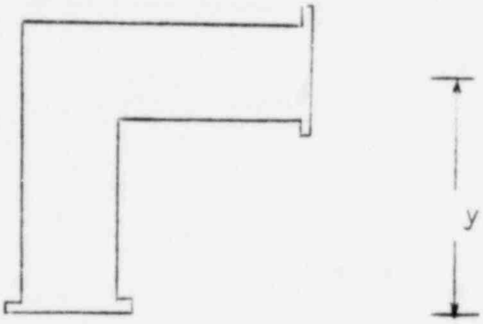
2.0 INLET FLANGE AND BOLTING ANALYSIS

2.1 Purpose

The ability of the inlet flange and bolting to withstand a discharge thrust force due to ATWS pressures has been evaluated with the following hand calculation.

The thrust force is ratioed to a 4300 psi inlet pressure. The flange and bolting are evaluated in accordance with the ASME Code.

2.2 Data



From Reference B-3

Pr_{des} = 2500 psi

F_d = 31700 psi where the momentum term equals 7000 lbs and the remainder is due to 500 psi backpressure

For a pressure of 4300 psi, assume the momentum force is scaled to the pressure while the backpressure force is constant.

$$F_d = \frac{4300}{2500}\ (7000) + 24700 = 36750 \text{ lbs}$$

2.3 Evaluation

Sect. III, ASME Code: NB-3658 Analysis of Flanged Joints.

1. Level C Limits: See NB3658 for definition of terms

$$M_a \leq [11{,}250A_b - (\tfrac{n}{16})\ D_f^2\ P_{fd}]\ C(\tfrac{Sy}{36})$$

$$\leq [11{,}250(21) - (\tfrac{8}{16})\ (8.25)^2\ (4300)]\ (14.5)\ (\tfrac{18.1}{36})$$

$\leq$ 655,510 in-lbs (Maximum Allowable Moment on Inlet Flange)

Maximum predicted moment, $M_c = F_dY$ = 482,144 in-lbs

$M_c < M_a$: The results are therefore acceptable.

2. Bolting

Stress in bolts due to 4300 psi = $\frac{W_{m_1}}{A_b}$

$W_{m_1} = 0.785\ G^2P + (2b \times 3.14\ GmP)$

G = 7.5
b = 0.75
m = 3

$W_{m_1} = 645{,}800$ lbs

$S_b = 30.75$ ksi

Allowable Stress = 2S = 53.6 KSI

The results are therefore acceptable.

3.0 REFERENCES FOR APPENDIX B

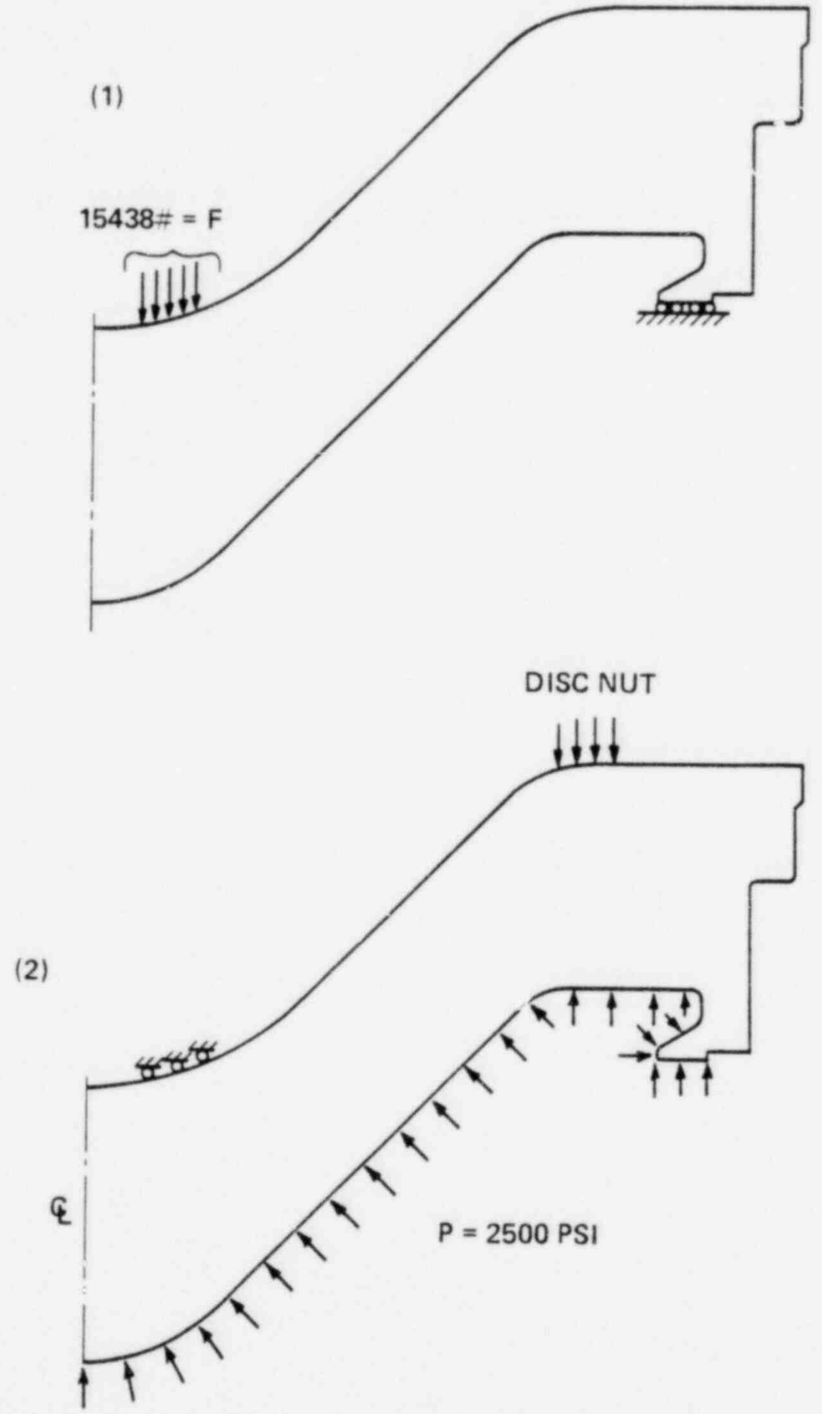
B-1 MARC-CDC, "Non Linear Finite Element Analysis Program," Revision H, 1976, Control Data Corporation, Minneapolis, Minnesota.

B-2 ASME Section III, Division I, 1977 Addition Through Winter Addenda.

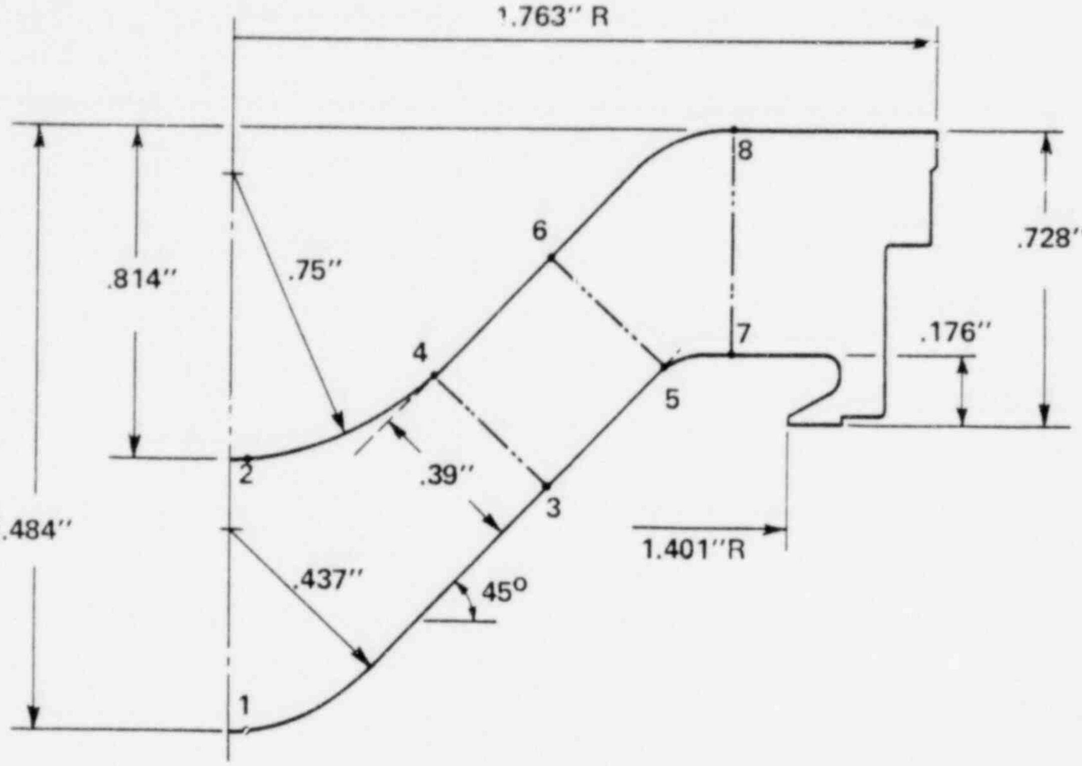
B-3 Dresser Design Report, 74-317-10 Revision 02.

B-4 Dresser Pressurizer Safety Valve Drawing 3NC-007.

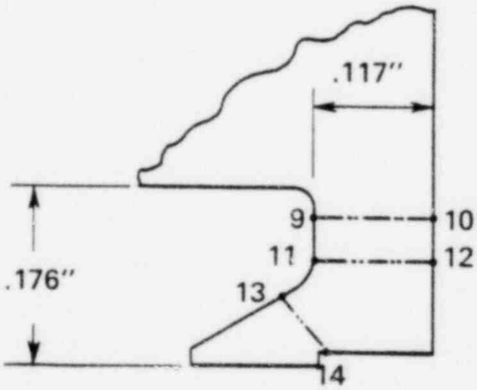
B-5 Dresser 31709NA Pressurizer Safety Valve Stress Report: Dresser Document No. 755-317-14, Revision 2, dated October 2, 1975.



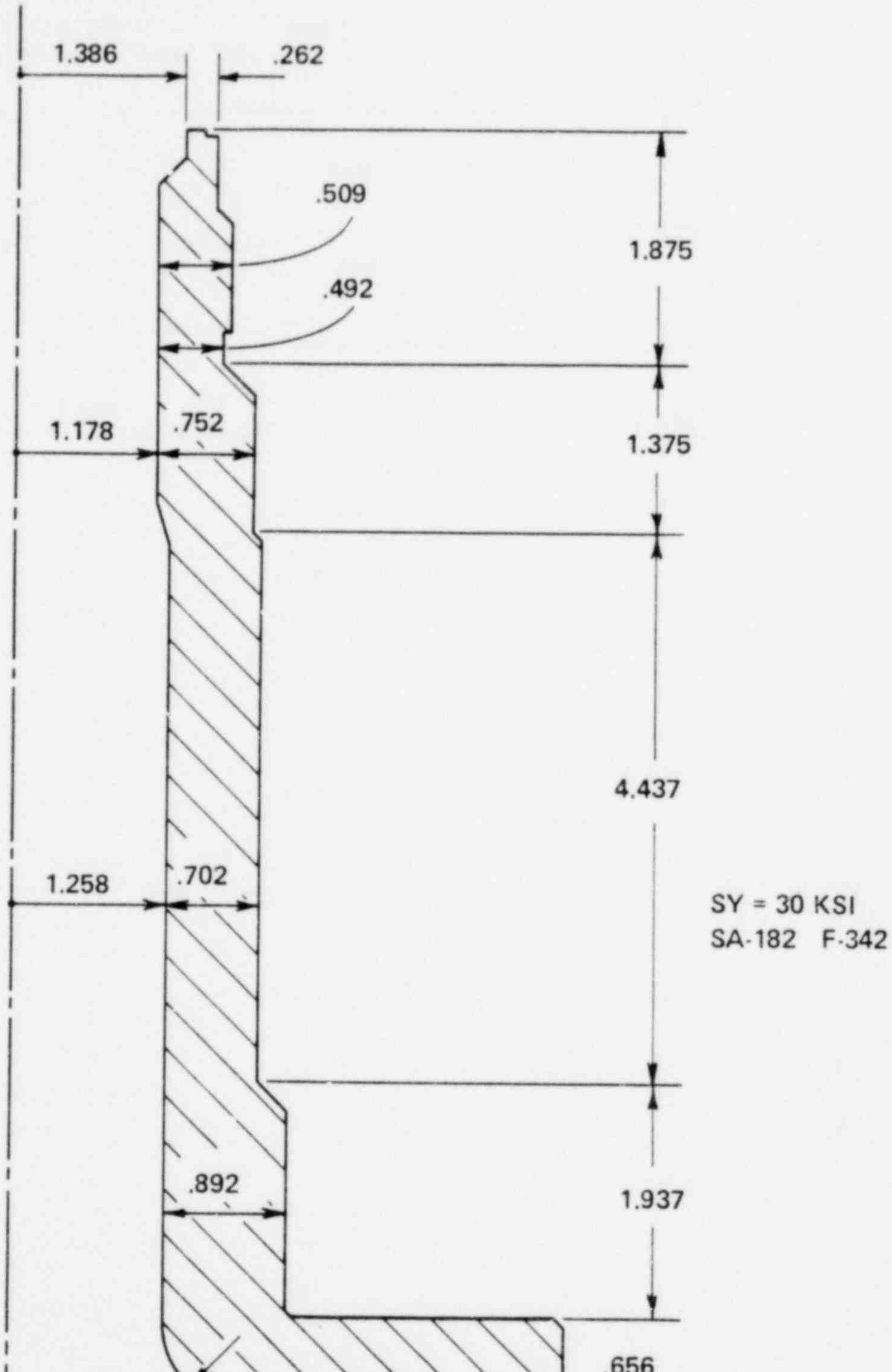
CEN-134-P	DISC LOADING	Figure B-1



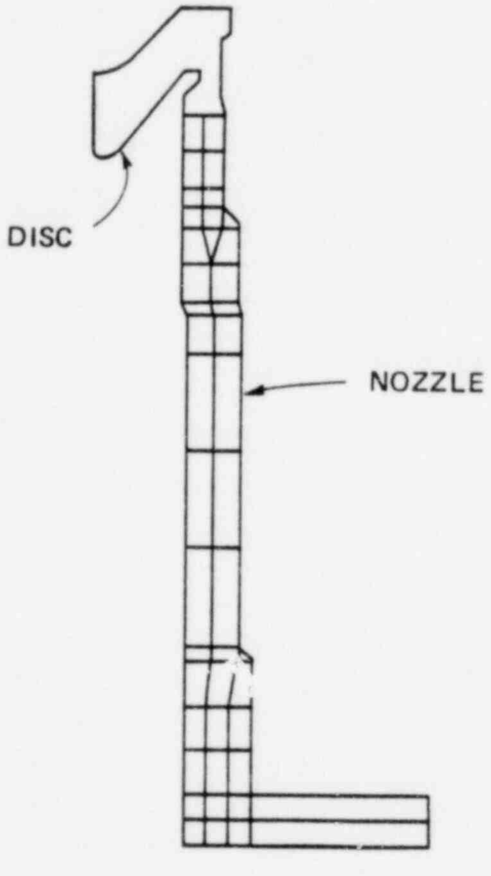
SY = 115,000 PSI
MAT'L: ASTM A-637
GR. 688 TYPE 2
SM = 51,000 PSI @ 700°F



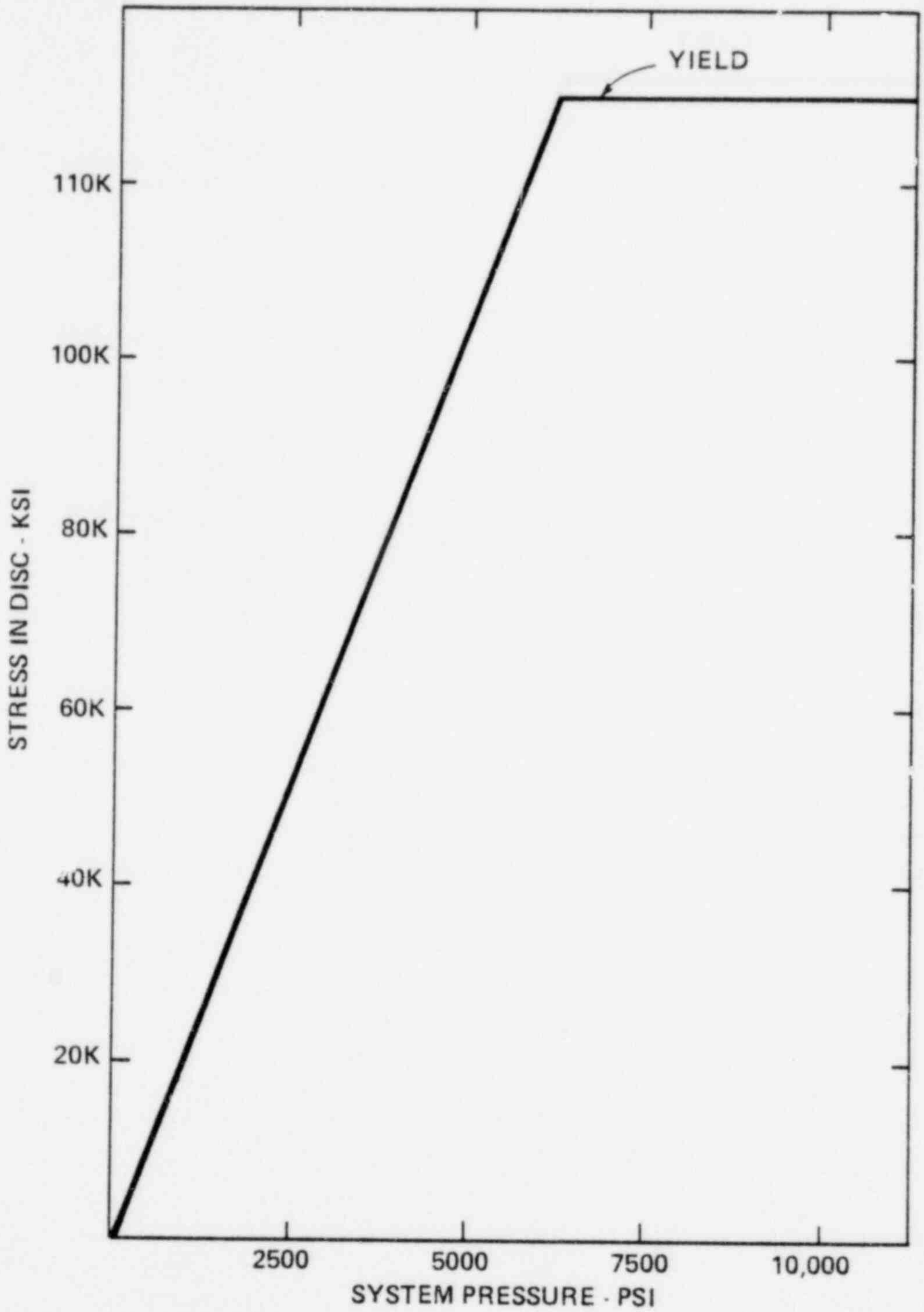
CEN-134-P	DISC GEOMETRY	Figure B-2



CEN-134-P	NOZZLE GEOMETRY	Figure B-3

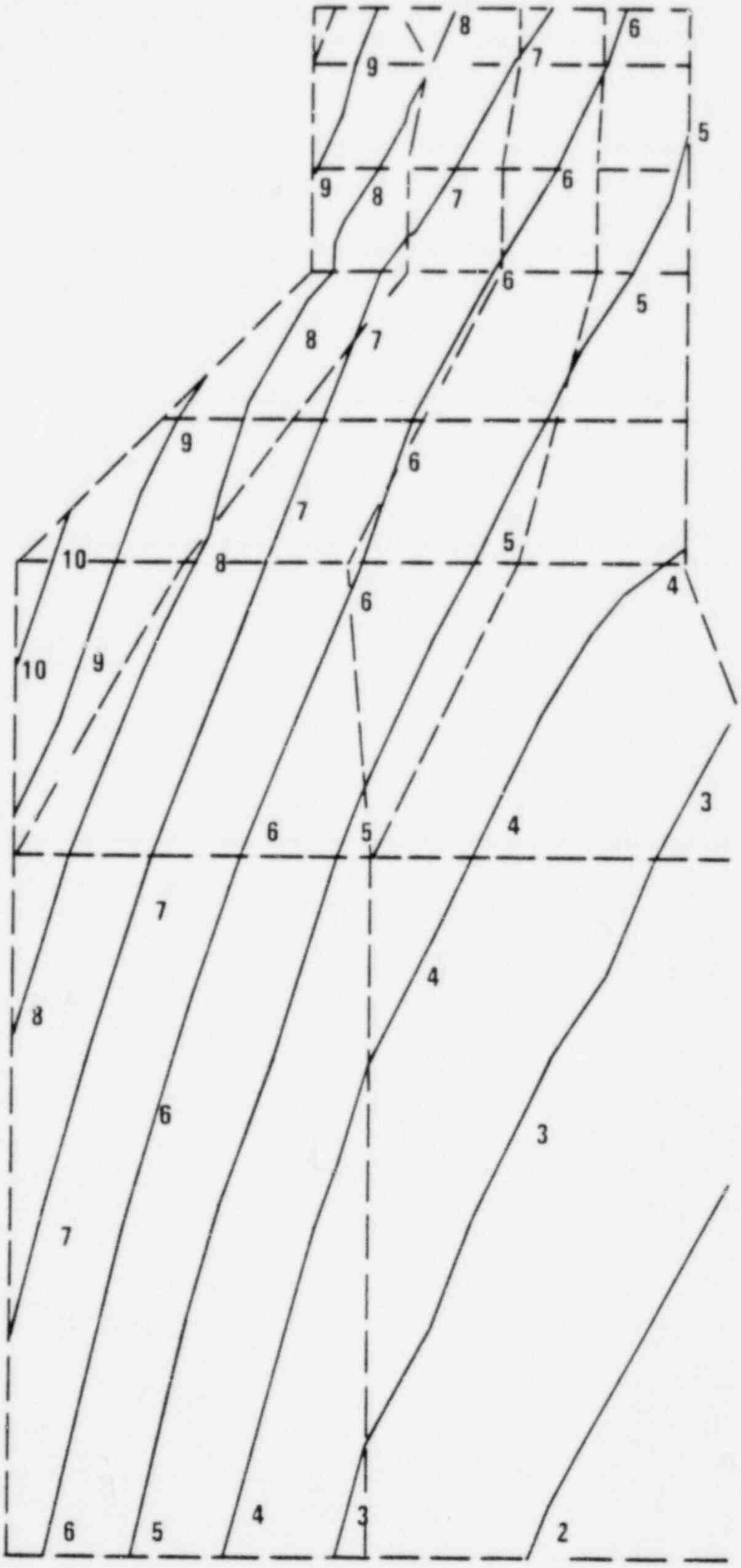


CEN-134-P	FINITE ELEMENT MODEL	Figure B-4

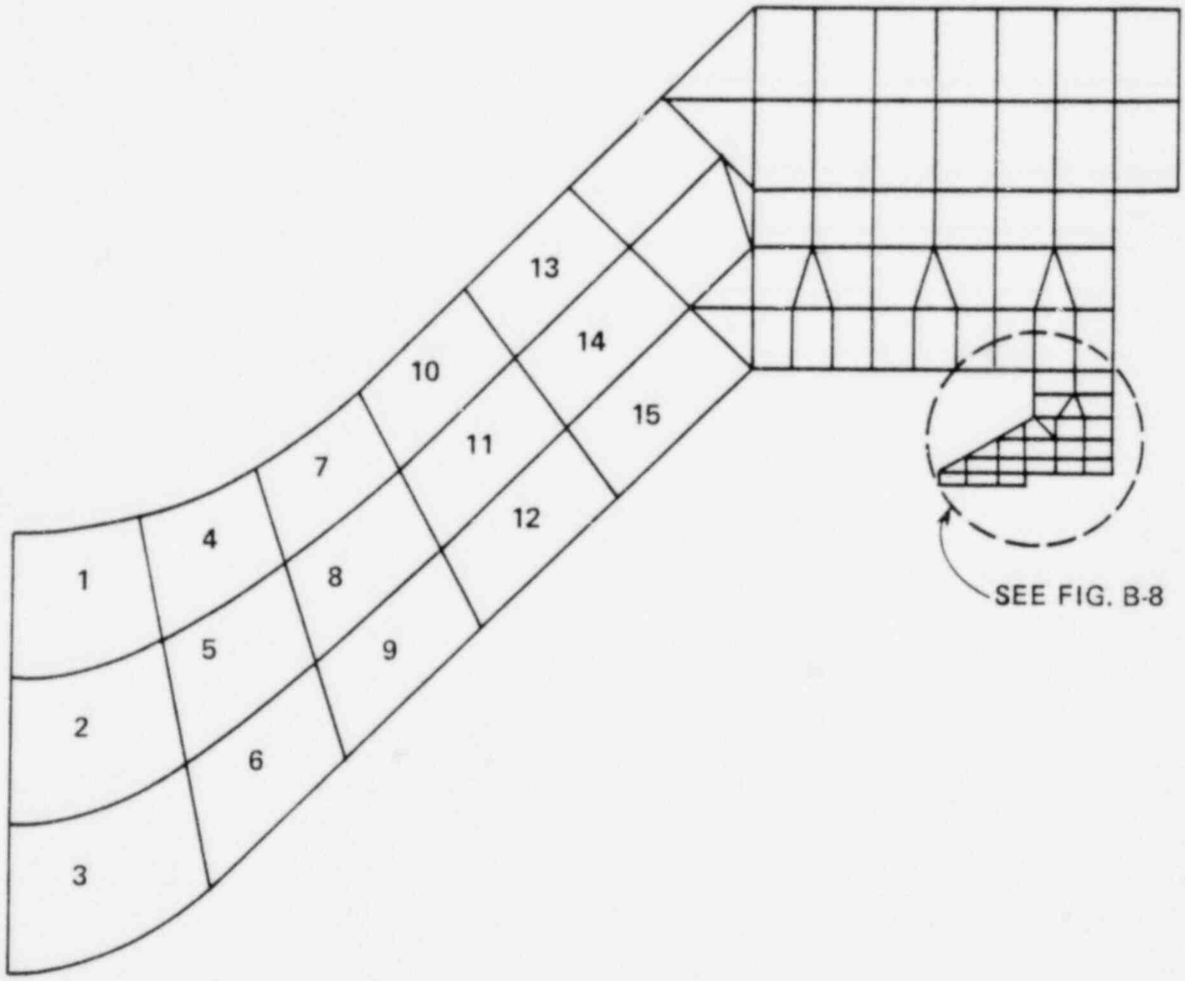


CEN-134-P	DISC STRESS vs PRESSURE	Figure B-5

1	=	.138	E5
2	=	.155	E5
3	=	.172	E5
4	=	.189	E5
5	=	.286	E5
6	=	.223	E5
7	=	.240	E5
8	=	.258	E5
9	=	.275	E5
10	=	.292	E5



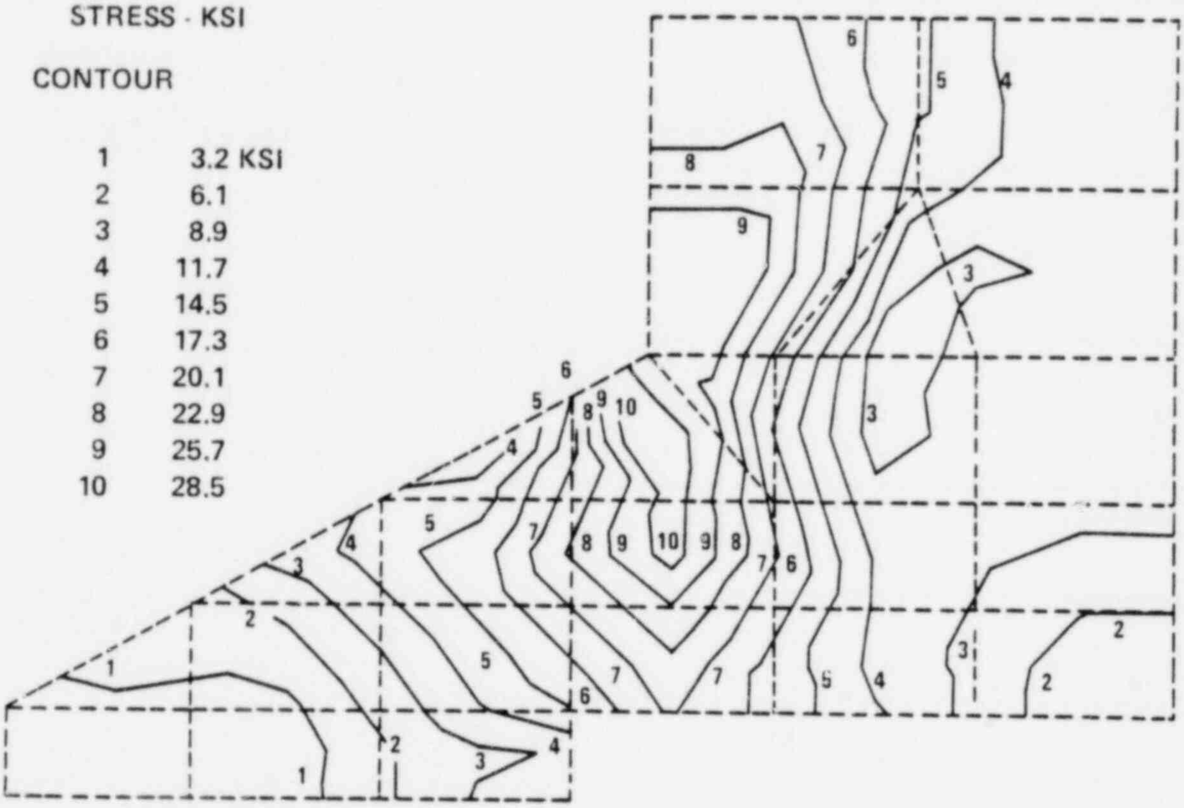
CEN-134-P	STRESSES IN NOZZLE P = 6350 PSI PRESSURE (VAN MISES EQUIVALENT STRESS)	Figure B-6



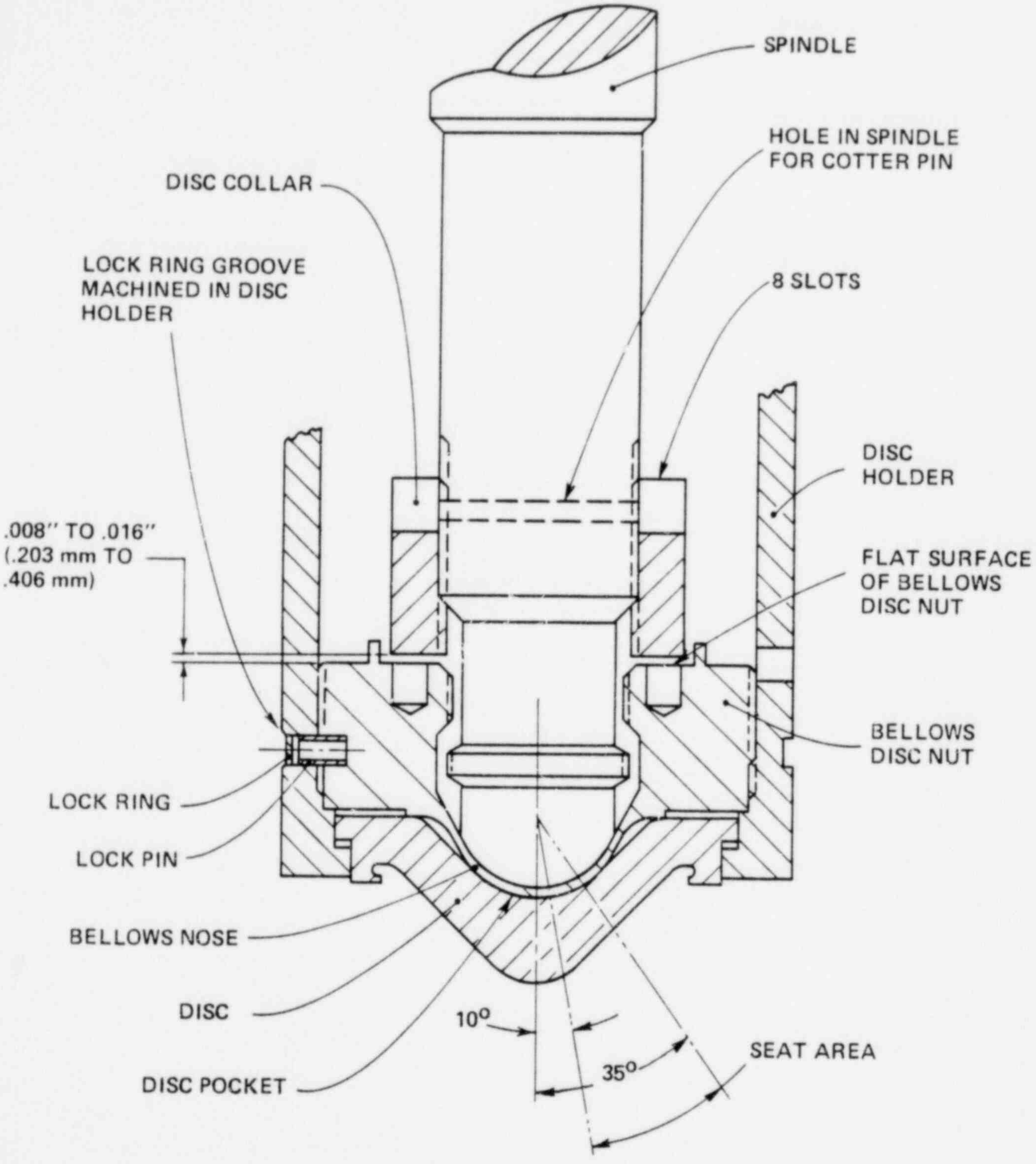
CEN-134-P	FINITE ELEMENT MODEL DISC	Figure B-7

STRESS - KSI

CONTOUR	
1	3.2 KSI
2	6.1
3	8.9
4	11.7
5	14.5
6	17.3
7	20.1
8	22.9
9	25.7
10	28.5



CEN-134-P	STRESSES - DISC POINT LOAD = 15438 LBS	Figure B-8



CEN-134-P	SPINDLE-BELLOWS-DISC ASSEMBLY, LOCK PIN DESIGN	Figure B-9

Appendix C

MTC for ATWS Analyses

1.0 INTRODUCTION

The purpose of this appendix is to supply additional information and explanation on the behavior of reactivity coefficients in C-E reactors. This appendix is intended to provide background information for the NRC Staff to provide a perspective on the issue of MTC and improve understanding of the differences in MTC between different classes of PWR reactors.

2.0 SUMMARY

Moderator temperature coefficient has been consistently identified as a major parameter in determining the sensitivity of an NSSS to an ATWS event. The major MTC differences between classes of reactor can be attributed to current operational differences, e.g., use of lumped burnable poisons, cycle length, and core average moderator temperature. Indeed, these differences are relatively small and recent calculations (CENPD-263) have shown that the associated effects on predicted peak ATWS pressure are relatively small for C-E plants.

3.0 DEFINITIONS

The following definitions are supplied in order to clarify the terminology which will be used in this appendix.

(a) Moderator Temperature Coefficient (MTC)

MTC is the change in core reactivity for a unit change in moderator temperature, with all other parameters (fuel temperature, power, pressure) held constant.

(b) Fuel Temperature Coefficient (FTC)

FTC is the change in core reactivity for a unit change in fuel temperature, with all other parameters (moderator temperature and pressure) held constant.

(c) Isothermal Temperature Coefficient (ITC)

ITC is the change in core reactivity for a unit change in both fuel and moderator temperature with all other parameters (power and pressure) held constant.

i.e. ITC = MTC + FTC

Moderator temperature coefficient can conveniently be broken down into two components, viz. the kernel (or spectral) temperature coefficient and the moderator density coefficient. These two components will now be defined.

(d) Kernel Temperature Coefficient (KTC)

KTC is the change in core reactivity for a unit change in moderator temperature at a constant moderator density, i.e. pressure changes to compensate for the change in moderator temperature. It is the reactivity associated with the changes in the neutron energy spectrum caused by a change in moderator temperature. Fuel temperature, moderator density and power are assumed to remain constant.

(e) Moderator Density Coefficient (MDC)

MDC is the change in core reactivity for a unit change in moderator density at a constant moderator temperature. It is a measure of the reactivity associated with changes in moderator density which affect both the neutron slowing-down (moderation) and neutron absorption in the soluble boron. Fuel temperature, moderator temperature and power are assumed to remain constant. Conceptually,

$$MTC = KTC + MDC$$

4.0 PARAMETRIC VARIATIONS OF REACTIVITY COEFFICIENTS

The Kernel Temperature Coefficient has been found to be almost independent of both moderator temperature and moderator density. The calculated value is found to be dependent on both the concentration of lumped burnable poison and the presence of plutonium isotopes. Consequently, the value is found to be exposure dependent. Typical values for KTC are $-0.23 \times 10^{-4}\ \Delta\rho/°F$ at beginning of core life and $+0.26 \times 10^{-4}\ \Delta\rho/°F$ at beginning of an annual UO_2 equilibrium cycle. The value of $+0.26 \times 10^{-4}\ \Delta\rho/°F$ was chosen for the CENPD-263 analysis as being a representative value for the calculated ATWS MTC.

The Moderator Density Coefficient is principally a function of the soluble boron concentration in the moderator. Changes in moderator density alter the boron number density and hence amount of absorption in the soluble poison. The MDC is also a strong function of moderator density and varies most rapidly as the system approaches saturation conditions. The MDC is a trade-off between two factors.

(a) The change in neutron moderation as density changes. Moderation increases as density increases and hence contributes a positive component to the coefficient. (Note that density coefficients have the opposite sign to that of temperature coefficients).

(b) The change in soluble boron absorption as density changes. Absorption increases as density increases and hence a density increase has a negative reactivity effect. Thus the soluble boron component contributes a negative component to the density coefficient.

The sign and magnitude of the density coefficient is therefore dependent on the concentration of soluble boron in the moderator. At high boron concentrations, the density coefficient will tend to be negative and at low boron concentrations the coefficient will tend to be positive. Any change in the lattice or fuel isotopics which reduces the need for soluble boron will produce a more positive density coefficient. It should be noted that any change in isotopics or lattice design will also affect the kernel coefficient; however, these changes are usually much smaller than the impact of soluble boron reduction. Examples of changes which influence the soluble boron concentration are as follows:

(a) Xenon concentration: as xenon builds up to equilibrium values, the need for soluble boron to hold down reactivity decreases and the density coefficient becomes more positive.

(b) The addition of lumped burnable poisons (e.g. B_4C shims) reduces the need for soluble boron and produces a more positive MDC.

(c) The need for soluble boron can also be reduced by inserting control rods to compensate for the reduction in soluble boron.

(d) A burnable poison such as gadolinium can be added to selected fuel rods in order to reduce the reactivity holddown required from the soluble boron.

(e) Core burnup (fuel depletion plus fission product buildup) provides a natural decrease in core reactivity. As a result the moderator density coefficient becomes steadily more positive as core burnup increases.

Table C-1 summarizes the sensitivity of the moderator reactivity functions to various reactor parameters. Figure C-1 illustrates the dependence of moderator temperature coefficient on soluble boron level. The figure is derived from actual measurements on C-E's operating reactors and includes data from cycles 1 through 4 and includes data at both hot full power and hot zero power. The strong relationship between moderator temperature coefficient and soluble boron level can be seen despite the wide diversity of reactor conditions represented in this data.

Fuel temperature coefficients are a function of fuel temperature. Since fuel temperature is a function of reactor power level, the fuel temperature coefficient is a function of reactor power level. Fuel temperature also rises as the moderator temperature rises. This means that in any transient in which moderator temperature changes, some account must be taken for the reactivity associated with the consequent change in fuel temperature. This implies that reactivity changes (at constant power level) are more aptly described by the isothermal temperature coefficient rather than the moderator temperature coefficient. The fuel temperature coefficient varies slightly with fuel burnup and also with irradiation cycle.

5.0 CALCULATION METHODS

For reactor conditions in which the void content is either negligible or can be assumed to be relatively uniform, moderator reactivity coefficients can be accurately calculated using two-dimensional planar models of the reactor. Either coarse mesh methodology (ROCS) or fine mesh calculations (PDQ) can be employed. In the CLOF transient reported in CENPD-263, there is negligible void formation prior to the time of peak pressure and therefore this approach is valid.

If significant void formation is expected, a realistic void distribution will best approximate the void reactivity feedback. The amount of void feedback will then depend upon the history of temperature and pressure during the transient. In such cases it may not be possible to identify a single density function: the appropriate reactivity function will depend upon the power, pressure and temperature history of the transient.

6.0 COMPARISONS WITH B&W AND WESTINGHOUSE PLANTS

The C-E lattice design has been shown to have an intrinsic moderator temperature coefficient which is less negative than the corresponding lattice of other PWR designs, by about 0.1 to 0.2 x 10^{-4} $\Delta\rho/°F$. This difference is believed to result from the larger CEA guide tubes in the C-E design which result in large thermal flux peaking in the region of these guide tubes. This peaking causes an increased neutron flux-weighting of the moderator in that region and consequently a stronger dependence of the reactivity on moderator density. In the presence of relatively high boron concentrations this produces a more negative density coefficient than would be obtained for other corresponding PWR designs. It has been shown that the kernel coefficients associated with the lattice designs are, in fact, comparable.

Given that the intrinsic differences in MTC among the PWR vendor designs are small, i.e. $\sim$ 0.1 to 0.2 x 10^{-4} $\Delta\rho/°F$, the actual differences in the values assumed in the ATWS analysis are the result of operational, technical specification, and fuel management differences. The following is a partial list of contributing factors which can explain most of the differences between classes of plants and among the PWR NSSS vendors.

1) C-E's 2560 Mwth class operates at a core average moderator temperature of 573°F in contrast to 583°F and 594°F for the 3410 and 3800 Mwth classes, respectively. From this alone, one would expect the 2560 Mwth Class MTC to be less negative than 3410 and System 80 classes by 0.13 and 0.25 x $10^{-4}\Delta\rho/°F$, respectively.

2) Operation of reload cycles without the use of lumped burnable poisons is chosen to result in high uranium utilization and lower average linear heat rate but it leads to high soluble boron requirements at BOC and hence more positive MTC's.

3) Operation in an unrodded mode increases thermal margin, increases shutdown margin, decreases the probability of PCI problems and reduces monitoring problems. However, the systematic use of substantial control rod insertion during operation does reduce the need for soluble boron and produces a more negative MTC.

4) Cycle length increases are expected to result in more negative MTC values in the equilibrium cycle. However in transition cycles, the coefficient may be more positive unless additional burnable poisons are added to compensate.

5) In the definition of a 95% or 99% probability MTC there is an uncertainty of at least ± 0.1 x $10^{-4}\Delta\rho/°F$ in defining the appropriate value. In particular, the assumptions that are made concerning future operating modes and frequencies of operations, fuel management schemes and lumped burnable poison usage affect the calculation of the MTC.

The differences in ATWS MTC's reported in NUREG-0460, Volume 4, can be explained through a combination of the effects discussed above. The following list shows how these effects influence the MTC for C-E's 2560 Mwth Class (assuming annual UO_2 reload cycles):

(1)	ATWS MTC (CENPD-263) Base Value	-0.20 x $10^{-4}\Delta\rho/°F$
(2)	Large guide tubes vs. distributed guide tubes	∿ +0.20 x $10^{-4}\Delta\rho/°F$
(3)	14 x 14 lattice vs. 16 x 16 assembly lattice	∿ +0.15 x $10^{-4}\Delta\rho/°F$
(4)	Moderator temperature 594°F to 572°F.	∿ +0.25 x $10^{-4}\Delta\rho/°F$
(5)	Unshimmed reload fuel batches (assuming 2% shims loaded)	∿ +0.25 x $10^{-4}\Delta\rho/°F$
(6)	Unrodded operation vs. operation with 1% control rods inserted	∿ +0.39 x $10^{-4}\Delta\rho/°F$

This approximate analysis illustrates the relative sensitivity of the MTC to assembly design parameter (items 2 and 3) compared to operational parameters (items 4, 5, and 6). Items 4, 5, and 6 are open to manipulation to achieve a desired MTC value. However, it should be clearly understood that this would be at the expense of uranium utilization, thermal margin, operating flexibility and monitoring capability.

7.0 DEFINITION OF AN MTC FOR ATWS

As defined above, MTC is only valid under constant pressure conditions. Therefore, considering the nature of the transients analyzed for ATWS, MTC should not be chosen as the sole parameter used to characterize the core feedbacks. It is more appropriate to distinguish kernel temperature coefficient and moderator density coefficients used in the analyses. Moreover, under voided conditions the moderator density coefficient will be a function of both the void content and the void distribution and will not be well correlated with the hot full power MTC. The hot full power MTC is only indicative of the moderator feedback under conditions in which the reactor coolant remains significantly subcooled.

It is simplistic to suggest that the beginning of life, hot full power MTC is a basic characteristic of any class of plant. There are a number of methods available by which a more negative MTC can be achieved. However, the benefits to be gained from a more negative MTC, in terms of reduced ATWS pressure, would have to be balanced against the costs of less efficient fuel management designs and operational modes.

TABLE C-1

Parametric Variations of Reactivity
Coefficients Calculated for PWR's

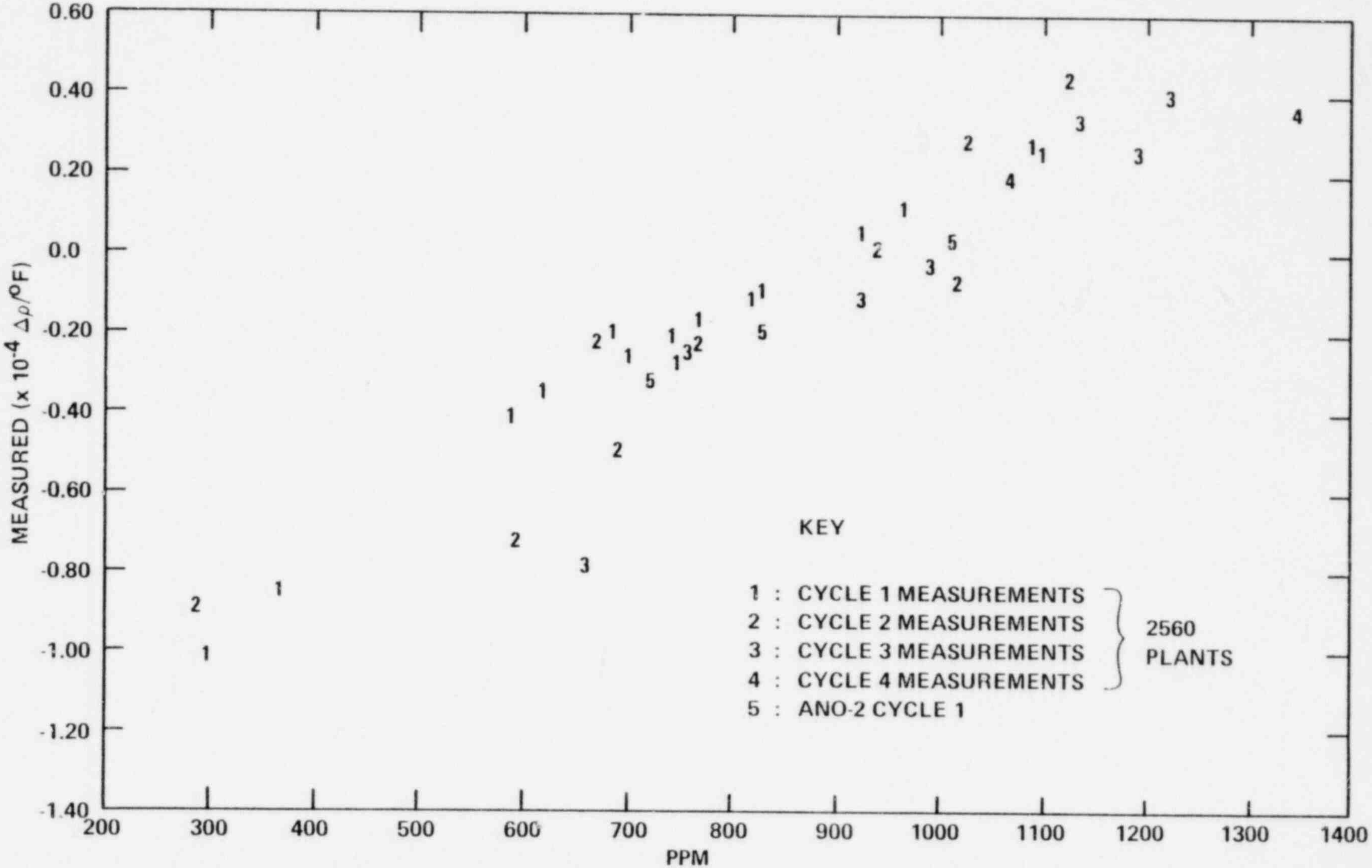
PARAMETER SENSITIVITY

Coeff.	Moderator Temperature	Power	Moderator Density	Burnup	Soluble Boron
KTC	No	No	No	Yes	No
MDC	Yes	No	Yes	Slightly	Yes
FTC	No	Yes	No	Slightly	No

CEN-134-P

MEASURED ITC
C-E OPERATING PLANTS

Figure C-1



APPENDIX D

STEAM GENERATOR TUBE TESTS

1.0 SUMMARY

This appendix documents the leak rate and burst tests performed on virgin, wasted, and wasted with Electric Discharge Machine cracks tube specimens. All tube specimens were acquired from one steam generator tube of Inconel 600 material having a yield strength of 40,000 psi, a tensile strength of 91,000 psi and an elongation of 49% in two inches. The tube was nominally 0.750" O.D. X 0.048" average wall thickness.

A total of 27 tube specimens were prepared for test purposes. Selected tubes were machined to simulate uniform wastage (nominal 64% of tube wall removed) as shown on C-E Dwg. D-62676-003 which is located in this section of Reference D-1.

Approximately 1/2 of the tube specimens were prepared with cracks. The cracks were machined using an Electric Discharge Machine (EDM). A 1/4" x 0.005" silver solder electrode was used. Since the electrode is always discharging as it progresses through the metal, this provides a larger crack opening at the top than at the bottom. In some instances a variation in crack width also occurred. The crack width ranged from 0.007" to a maximum of 0.014" at the bottom of the crack. All crack measurements are documented in Reference D-1.

2.0 TESTING

A brief discussion of the results of the various tests is provided below:

<u>Burst Tests</u> - Both simulated wasted and virgin tubes were ruptured under water pressure at room temperature conditions. Two of the wasted tubes were subjected to bending load such as to produce a maximum stress in the outer fiber equal to 35 KSI. The burst test results are provided in Table D-1 of this section of the report.

Based on this data (2 tube samples of each type) it may be concluded that the average burst pressure is reduced approximately as shown in Table D-2 compared to the virgin specimen for the various conditions.

3.0 REFERENCES

D-1 CENC-1256, "Steam Generator Tube Tests", dated February, 1976.

Table D-1

Tube Burst Tests

(Palisades)

Tube Size - 0.750" O.D. X 0.048" W.T., Material - Inconel 600

Specimen No.	Configuration No.	Brief Description of Specimen	Burst Pressure
1-1	1	1/2" long wastage all around	7400
3-1	1	1/2" long wastage all around	7450
*1-2	2	5/16" long wastage all around	*7700
*2-2	2	5/16" long wastage all around	*7900
4-3	3	5/16" long wastage all around	7800
5-3	3	5/16" long wastage all around	8000
3-4	4	1/2" long dished wastage one side	7000
4-4	4	1/2" long dished wastage one side	7500
1-6	6	Virgin Tube	11,900
2-6	6	Virgin Tube	11,100

*Bending load applied such as to produce bending stress equal to 35 ksi.

TABLE D-2

Tube Burst Ratios

Spec. Config. No.	Description	Ratio of Specimen Burst Pressure to Virgin Tube Burst Pressure
1	1/2" wasted all around (.017" MWT)	65%
2	5/16" wasted all around with bending load (.017" MWT)	68%
3	5/16" wasted all around (.017" MWT)	69%
4	1/2" long dished wastage, one side (.017" MWT)	63%

Appendix E

Description of Reactor Vessel Closure Analysis

1.0 INTRODUCTION

The interaction behavior of the reactor vessel head, the reactor vessel flange, the head hold-down bolts, and the self-energizing O-ring seals have been evaluated for hydrostatic testing, initial preload and internal pressure loadings from zero up to estimated 4300 psi ATWS levels. This evaluation includes both linear and non-linear behavior and has been performed with the MARC computer program (Ref. E-1).

The hold-down bolts are initially preloaded during hydrostatic testing to a higher preload than is used for normal operations. Thus the head and vessel flange interface is subjected to large compressive forces which result in plastic deformation of the cladding on these surfaces. The preloading for normal operation also plastically deforms the O-rings resulting in a tight sealing of the reactor vessel. When the vessel is pressurized to levels above [ ] psi, the vessel head and flange begin to separate while the O-rings continue to maintain a tight seal. [

] As the vessel head begins to separate from the flange, the O-rings mechanically "spring back" from their compressed state. Pressurization of the inner O-ring following initial flange separation results in a slight expansion of the O-ring tending to maintain the seal at this connection.

Further pressurization of the vessel causes the hold-down bolts to continue to stretch elastically. Leakage about the inner O-ring occurs when the head and vessel flanges have separated a greater distance than the O-ring spring back and expansion. [

] Once pressure relief is initiated past the inner O-ring, the increased surface area of the head which is exposed to the pressure loading results in a rapid opening of [ ] After this occurs, relief area increases linearly with pressure since the structure responds elastically during further pressurization of the vessel.

2.0 O-RING CHARACTERISTICS

The integrity and operability of the reactor vessel O-ring, for System 80 and St. Lucie 1, are evaluated after it is exposed to ATWS pressure. The ring is analyzed for initial preload in the vessel hold-down studs and internal pressure loadings from zero to estimated ATWS levels.

2.1 Geometry

The geometry of the O-ring is as shown in Figure E-1.

Material: SB-178 Inconel Alloy 718

$Sy = 162500$ Psi

$E = 29.5 \times 10^6$ Psi

2.2 Analysis

The model is formulated using [ ] the MARC program (Reference E-1). The ring is subdivided into 72 elements. The analysis is divided into two parts:

In the first part, the ring is compressed diametrically by increasing the load, in the hold-down studs, in discrete steps. In order to follow the load-deflection curve as shown in Figure E-8 (Reference E-2), the work hardening process as described in MARC (Reference E-1) is used. In the final stage, [ ]

In the second part, the internal pressure load is applied. The pressure load is increased in small steps starting from zero pressure to estimated ATWS levels.

2.3 Results

The preload on the hold-down studs induces plasticity in the O-rings. The area of the ring which is in contact with the flange and vessel head is deformed more than other areas of the ring. (See Figure E-6.)

When the pressurization of the vessel begins, the studs stretch, the compression between head and flange of the vessel is reduced, and the O-rings spring back slightly from the preload shape. This is shown from step 10 to 11 in Figure E-2. [ ] Pressurization of the inner O-ring following the initial opening of a gap between the head and the flange of the vessel results in a slight expansion of the O-ring tending to maintain the seal. This behavior of the O-ring is shown from step 11 to 18 in Figure E-2. Further pressurization of the vessel causes the separation of the inner O-ring from the head and flange of the vessel. The inner O-ring is not able to seal the leakage. This behavior of the ring is shown from step 18 to 19.

The sensitivity of the O-ring is clearly seen from step 10 to 18. [

] Again, initial pressurization of the vessel will reduce the compression . the O-ring, and the O-ring will spring back slightly from the deformed shape. But, step 11 will rise to a new position from the previous position. Further pressurization of the vessel will expose the O-ring to the pressurization fluid. The O-ring is filled with the pressurized fluid which will expand the O-ring diametrically. This increases the sealing capacity of the O-ring. The step 18 will rise to a new position. Further increase in fluid pressure will release the contact of the O-ring from the flange and head of the vessel.

[

] When the vessel is pressurized, the outer O-ring is not able to self-energize and expand. It can be seen from Figure E-3 (point 12) that the maximum load carried by the outer O-ring
occurs during the preload stage. When the vessel is pressurized from the preload to the estimated ATWS pressure, the outer O-ring loses its effectiveness because [

] It is seen that the outer O-ring behaves in a similar fashion when the preload on the bolts is reduced to zero and when the vessel is pressurized from the preload stage. It is seen that the inner O-ring behaves differently when the bolt preload is reduced to zero and when the vessel is pressurized from the preload stage. This is the distinct difference in behavior of the inner and outer O-rings. [

]

3.0 REACTOR VESSEL HEAD AND FLANGE ANALYSIS

3.1 Introduction

The effect of ATWS pressures on the reactor vessel O-ring seal has been evaluated by elastic plastic finite element analysis. Detailed analyses of both the St. Lucie vessel, representing the 2560 Mwt class of vessels, and the System 80 vessel were performed. The analysis for the 2560 Mwt class vessel only will be discussed in detail.

The analysis consisted of considering bolt up prior to vessel hydro-test, unloading due to unbolting and subsequent bolt up for operation. Heatup and pressurization to operating pressure and up to ATWS pressures were then performed. The elastic plastic temperature dependent properties of the stainless steel cladding in the seal region were used in order to precisely define the deformation of both the O-ring and the head and flange mating surface.

The analysis of the effect of the head and flange deformation and motion on the O-ring behavior was performed. The force the O-ring exerts on the head was neglected. This effect is small and neglecting it is conservative for predicting the highest pressure at which substantial leakage will occur since it tends to open the gap between the head and flange.

3.2 Model and Materials

The finite element model of the head flange region is shown in Figure E-9. The model is axisymmetric except for the following sections:

The elastic properties of the head are modified to consider the reduction in stiffness due to the presence of the CEDM penetrations. The stress strain curves of the stainless steel cladding at 120°F and 550°F are shown in Figure E-10, and compared to experimental data. All other material properties are taken from the reactor vessel stress report, Component Catalogue 3-11.

3.3 Loadings

The loads are applied to the model to produce the effect of bolt tensioning and pressurization. [

] The stress in the bolt is monitored and loading is stopped when the desired preload stress is obtained.

The pressure is applied on all interior surfaces of the vessel and head. Pressure is also applied to the surfaces which separate during pressurization.

3.4 Results of Analysis

The bolt up prior to hydrotest produces plastic deformation in the cladding in the head-flange mating surface. Some additional plastic straining occurs during heat up after cold bolt up prior to operation. This strain results from the difference in thermal expansion of the cladding and the carbon steel bolts and base metal, and the change in yield stress of the cladding at the higher temperatures.

The change in the height of the O-ring groove as a function of bolt stress is shown in Figure E-11. This figure illustrates the amount of deformation which the O-ring would experience during bolt up for hydro-test. Values are indicated for the maximum and minimum bolt up tension. Also shown are the ring deformations for operational bolt up and after heat up just prior to pressurization. No plasticity occurs outside of the cladding in the head to flange mating surfaces.

3.5 Bolt Preload Tolerance - Simplified Calculation

The effect of pressurization on the seal is evaluated by both a simple procedure and by the detailed finite element method for both the maximum and minimum bolt preload. The simple procedure considers the preload on the flange caused by the bolts to be relieved by pressure in such a way that when the vertical force on the flange/head section is zero, only the seal is in contact and preventing leakage. The pressure for zero vertical force is:

$$\pi R_s^2 \quad P = N_B \times F_B$$

where P is the vessel pressure

R_s is the radius to the seal

N_B is the number of bolts

F_B is the bolt preload force: Figure E-11 and Table 3-7.

$$P_{Min} = \frac{54 \times 1231 \times 10^3}{\pi * (88.63)^2} = 2695 \text{ psi}$$

$$P_{Max} = \frac{54 \times 1359 \times 10^3}{\pi * (88.63)^2} = 2975 \text{ psi}$$

The pressure required to stretch the bolts to cause the O-ring to loose contact with the seal groove can be computed as follows:

$$S = \frac{F_B L}{AE} = \frac{\pi \Delta P R_S^2 L}{N_B AE}$$

where S = seal spring back

F_B = increase force in bolt

L = effective length of bolt

A = area of bolt

E = modulus of elasticity of bolt

$$\Delta P = \frac{S * N_B \times A \times E}{\pi R_S^2 L}$$

$$\Delta P_{Max} = \frac{.018 \times 54 \times 35.4 \times 27 \times 10^6}{\pi * (88.63)^2 * 30} = 1255 \text{ psi}$$

The pressures at which leakage occurs then are the sum of the separation and bolt stretch pressures.

P_{Min} = 3950 psi

P_{Max} = 4230 psi

These values are very conservative (high) since no consideration of the rotation of the vessel flange has been included. These pressures do establish, however, the range of values expected in the detailed analysis.

3.6 Finite Element Analysis - Bolt Preload Tolerance

The detailed finite element analysis results include stress and deformation at all locations in the vessel head and flange. [ ] The seal height during bolt up and heat up was described in Figure E-11. Since the cladding in the seal region is the only region where plasticity occurs and that region is unloading, the pressurization process is elastic. [ ]

The effect of pressure on the mating surfaces is shown in Figure E-12. For all pressures above [ ] psi, some separation of the head and flange exists. Increasing pressure decreases the contact surface on the inside and the outside of the region. Above [ ] psi or so the contact surface consists only of the cladding on either side of the inner O-ring. At [ ] psi the head and flange separate, leaving only the seal to contain the pressure. Near [ ] psi pressure the seal can no longer fill the space between the head and flange and leakage occurs.

[ ] is shown in Figure E-13. This figure shows a range of [ ] psi pressure for first significant leakage.

The finite element calculations were performed incrementally in order to determine the manner of separation of the mating surfaces. [ ]

This opening process is conservative for the opening area since it exposes surface only after an entire element would separate. [ ]

When the seal no longer fills the seal groove, the pressure acts on the remaining area of the mating surface. The outer seal is loose in its groove and, therefore, does not pressurize or prevent leakage flow. The increased pressurization surface causes a sudden "pop open" of the head. The minimum opening gap between the mating surfaces is shown in Figure E-14. The corresponding opening areas are shown in Figure E-15. The gap opening at [ ] psi internal pressure is about [ ] inches. The minimum diameter of the deformed O-ring is [ ] inches, as shown in Figure E-16. The much larger O-ring cannot be pressed into the very small gap. It is most likely that the O-ring will lift with the head (since it is pinned in the head for installation) and not interfere with the leakage flow. When the pressure is relieved and the head reseals on the flange, the elastic bolts assure a reasonably tight seal. The force per inch seal circumference that the bolts exert in closing from [ ] The seal collapse load is about [ ] lb/inch clearly indicating that the position of the seal makes little difference in the head closure.

4.0 "CLOSURE DOME EFFECT ON CEDMS"

The effect of closure head dome deformations and movements on control rod drive penetration housing welds have been considered. These welds could be loaded in two ways. First, the closure dome might be subjected to large strain inelastic deformations which would impose severe loadings on the housing penetration welds. Second, the lifted dome might vibrate at a frequency close to the natural frequency of the CEDMs. This would impose large dynamic forces on the CEDMs and overstress the welds.

The results of the detailed finite element analysis demonstrate that Level C stress criteria are satisfied in the closure dome for all loading conditions considered. The dome itself is not highly loaded and all deformations are in the elastic range. The CEDM welds are designed to resist seismically induced bending moments. The ATWS pressures do not impose any loadings which are as severe as the seismic design criteria.

The possibility of detrimental dynamic feedback to the CEDMs has been evaluated by comparing the natural frequencies of the closure dome and the CEDMs. The closure dome is a rigid body with a calculated natural frequency in excess of 250 Hz. The CEDMs are relatively soft bodies having natural frequencies less than 20 Hz. This difference in dynamic response characteristics will prevent head vibration from inducing large dynamic responses in the CEDMs.

5.0 PREDICTABILITY OF HEAD LIFT

The lifting of the vessel head and subsequent leakage of primary fluid past the O-rings is a result of elastic behavior of the closure region. The principal components which contribute to lifting are the stretching of the closure studs and the rotation of the closure head. Both of these elastic phenomena are well documented and predictable for pressure loading areas. The deformation behavior of the O-ring as the closure head begins to lift is also critical to the predictability of head lift. The O-ring analysis is presented in Section 2.0 of this appendix. This O-ring analysis has been compared to test data and is in close agreement with measured data.

Throughout the head lift evaluation, all variables, such as bolt load tolerance, have been chosen conservatively so as to predict leakage at higher pressures. The predicted lifting pressure is, therefore, shifted upwards from normal design values. The "smoothness" of the pressure vs. area curves, however, is due to the elastic behavior characteristic of the closure elements.

6.0 REFERENCES

1. MARC - CDC "Non-Linear Finite Element Analysis Program", Rev. H, 1976, Control Data Corp.

2. O-Ring Report (Chattanooga), "O" Ring Design and Manufacturing for Commercial Nuclear Reactor Vessels, C-E Report TIS-6174, February 1, 1979.

CEN-134 P	REACTOR VESSEL SEAL O RING FINITE ELEMENT CROSS SECTIONAL MODEL	Figure E-1

CEN-134-P	INNER RING 2560 MWt PLANT	Figure E-2

CEN 134 P	OUTER RING 2560 MWt PLANT	Figure E.3

CEN-134 P	SYSTEM 80 INNER RING	Figure E-4

CEN-134-P	SYSTEM 80 OUTER RING	Figure 1.5

CEN-134 P	REACTOR VESSEL SEAL RING AT THE END OF PRELOAD OF BOLTS	Figure E-6

CEN-134-P	REACTOR VESSEL SEAL RING FINAL SHAPE OF RING	Figure E-7

CEN-134-P	FORCE vs DEFLECTION FOR O - RING TEST SAMPLE	Figure E-8

CEN-134 P	VESSEL FLANGE O-RING SEAL LEAKAGE MODEL	Figure E-3

CEN-134-P	STRESS vs PLASTIC STRAIN	Figure E-10

CEN-134-P	GROOVE HEIGHT vs BOLT STRESS	Figure E-11

Sheet 1 of 2

CEN-134-P	O-RING LEAKAGE SEQUENCE	Figure E-12

Sheet 2 of 2

CEN-134-P	O-RING LEAKAGE SEQUENCE	Figure E-12

CEN-134-P	O-RING GROOVE HEIGHT	Figure E-13

CEN-134-P	2560 MWt PLANT GAP OPENING ERROR BAND	Figure E-14

CEN-134 P	2560 MWt PLANT FLOW AREA ERROR BAND	Figure E-15

CEN-134-P	O RING GAP, OPENED POSITION AT 4300 PSI	Figure F-16