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Team's Analyses of Soviet Designed VVERs

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Table of Contents

ANALYSIS APPENDIX A	RADIATION INDUCED EMBRITTLEMENT AND ANNEALING OF REACTOR PRESSURE VESSEL STEELS
ANALYSIS APPENDIX B	LOSS OF COOLANT ACCIDENT (LOCA) BLOWDOWN ANALYSES
ANALYSIS APPENDIX C	LOCA BLOWDOWN RESPONSE ANALYSES
ANALYSIS APPENDIX D	FUEL/CORE TEMPERATURE ANALYSES
ANALYSIS APPENDIX E	NON-SEISMIC STRUCTURAL RESPONSE ANALYSES
ANALYSIS APPENDIX F	SEISMIC ANALYSES
ANALYSIS APPENDIX G	"S" SEAL INTEGRITY
ANALYSIS APPENDIX H	REACTOR TRANSIENT ANALYSES
ANALYSIS APPENDIX I	FIRE PROTECTION
ANALYSIS APPENDIX J	AIRCRAFT IMPACTS
ANALYSIS APPENDIX K	BORIC ACID INDUCED CORROSION

ANALYSIS APPENDIX A

**RADIATION INDUCED EMBRITTLEMENT
AND ANNEALING OF PRESSURE VESSEL STEELS**

TABLE OF CONTENTS

	Page
Introduction	A-1
1.0 The Ductile-Brittle Transformation of Steels	A-1
2.0 Effects of Neutron Irradiation on Pressure Vessel Steels	A-2
2.1 Role of Material Chemistry	A-2
2.2 Role of Irradiation Temperature	A-3
2.3 Role of Dose Rate (flux)	A-3
2.4 Interactions Between Irradiation and Mechanical Factors that Influence T_{NDT}	A-3
2.5 Distinction Between Shift in T_{NDT} and Reduction in "Upper Shelf Energy (USE)"	A-4
3.0 Monitoring and Predicting Shifts in T_{NDT}	A-4
3.1 Sampling of the Vessel Material	A-4
3.2 Methods of Estimating Shifts in T_{NDT} from the Chemistry of Metals and Weld	A-4
4.0 Mechanisms of Radiation Enhanced Embrittlement	A-6
4.1 Copper Precipitation	A-6
5.0 Annealing of Radiation Effects in Pressure Vessel Steels	A-7
6.0 Field Experience and Proposals on Annealing of Pressure Vessels	A-8
7.0 Anticipated Benefits on Life Extension from Annealing	A-8
8.0 Embrittlement of VVER Pressure Vessels	A-8
Bibliography	A-12
Addendum 1	A-23
Addendum 2	A-24

TABLES

1. Composition of Nuclear Pressure Vessel Steels
2. Shifts in T_{NDT} , Degrees Centigrade for Steel Given in Table 1
3. Radiation Doses on Pressure Vessel Materials

ADDENDUM 2 TABLES

- A-1. Chemistry Factor for Welds
- A-2. Chemistry Factor for Base Metal

FIGURES

1. The Effect of Irradiation Temperature on Mechanical Properties of A533B Steel
2. The Effect of Dose Rate at Approximately 75 Degrees Centigrade on Shift in T_{NDT} of A212B Steel
3. Effect of Neutron Fluence on Different Mechanical Properties of Steel A, A533B, Irradiated at 290 Degrees Centigrade
4. Radiation Embrittlement of the Core Weld in Loviisa-1
5. Relative Recovery of Hardness as a Function of Annealing Time in Steel 15Cr₂MFA Loviisa-1 Specimens (T_{irr} : 265°C)
6. EPRI-Westinghouse Data on the Effect of Anneal Time on Recovery of Transition Temperature in Weld EP-24
7. Diagram of Change of Critical Brittle Temperature of 15Cr₂MFA Steel (a) and its weld Metal (b) Depending on the Neutron Fluence at Periodical Alternation of Irradiation at 285°C and Annealing at 420°C for 144 Hours.
8. (a) The Position of Dummies and the Main Azimuthal Directions
(b) Neutron Fluence in the Main Peak Directions
9. Effect of ΔT (Annealing Temperature Less Irradiation Temperature) on Recovery of Unirradiated Mechanical Properties of 15Cr₂MFA Steel

ADDENDUM 1 FIGURES

- 1A. Predicted Adjustment of Reference Temperature, "A", as a Function of Fluence and Copper Content.
- 2A. Fluence Factor for Use in the Expression for RT_{NDT}

ANALYSIS APPENDIX A

**RADIATION INDUCED EMBRITTLEMENT
OF PRESSURE VESSEL STEELS**

Introduction

Embrittlement of nuclear pressure vessel steels by neutron bombardment affects all light water reactors (LWR) in service to a greater or lesser extent. While this potential problem has been recognized since the beginning of the LWR industry, only recently have models for predicting it and techniques for controlling it been developed. Fundamental models for the embrittlement process are just beginning to be developed so reliance is placed mainly on semi-empirical models and interpreting them using the current understanding of radiation-induced precipitation processes and the fracture and ductility mechanisms in steels.

The purpose of this appendix is to review what is known about the phenomenon of radiation embrittlement, the techniques for controlling it, and for annealing to reduce the effects. Data on the embrittlement phenomenon and models for predicting the effect of a neutron flux or fluence on the ductile brittle transformation exist in both Western and Soviet literature. Models from both sources will be presented. Different materials are used in the U.S. and in the U.S.S.R. The compositions of typical pressure vessel steels used in the U.S. and in the Soviet Union are given in table A-1.

The VVER pressure vessels receive a relatively high fast fluence due to the relatively small amount of neutron moderation in the narrow water gap between the outermost rows of fuel and the vessel. Consequently, both embrittlement and annealing have been extensively studied for those reactors.

1.0 The Ductile-Brittle Transformation of Steels

Most metals have a range of temperatures in which they are ductile and fail by ductile rupture when stressed beyond their tensile strength. This can be described as a mechanism in which dislocation motion in the metal structure proceeds at the tip of an advancing crack as a result of the stress intensity from the presence of that crack. Rupture occurs by (1) nucleation of microvoids at second phase particles ahead of the crack, (2) their growth by plasticity, and (3) coalescence with each other and the crack tip.

At low enough temperatures, however, all body centered cubic metals and alloys, which include structural steels, reach a point at which a crack will propagate by cleavage (involving little dislocation motion) rather than by ductile rupture. Over a range of temperatures, which in some cases is rather narrow, the fracture mechanism abruptly transfers from one mode (ductile) to another (brittle) as the temperature is lowered. This is referred to as a ductile brittle transformation temperature. It is typically measured by determining the ability of standard notched specimens to absorb energy on impact in a Charpy V-notch (CVN) type test. International usage has accepted 41 joules (30 foot-pounds) as the point below which a structural steel is considered to be susceptible to brittle failure by cleavage. We will define this point as the "Temperature for the Nil Ductility Transformation" (T_{NDT}) for the purposes of this report. In the United States the ASME Code, Section III defines a reference temperature for nil ductility, RT_{NDT} , for use in the design of irradiation monitoring programs, which is taken as the greater of T_{NDT} or the temperature at which CVN specimens expand laterally upon testing ≥ 35 mils, less 60 degrees Fahrenheit.

The major point is that at temperatures at or below T_{NDT} the presence of flaws in the steel and a stress or a sudden impact loading such as may occur during low temperature pressurization or thermal shock may cause these flaws to propagate by brittle cleavage and potentially cause rupture or failure of the pressure vessel. Consequently, great care is taken to specify minimum pressurization temperatures for these vessels as the measured or predicted T_{NDT} plus 60 to 90 Fahrenheit (33 to 50 degrees Centigrade) above the nil ductility transformation temperature to prevent rupture or flaw propagation in the pressure vessels. The nil ductility transformation in typical pressure vessels steels in the absence of irradiation is a function of chemistry of the material (that is alloying constituents), any residual stresses in the material from welding, and any cold work in the material as a result of forming processes. If the completed pressure vessel is properly heat treated, the initial nil ductility temperature should be well below 0 degrees Centigrade. T_{NDT} can, however vary widely within a single steel plate or forging as a result of segregation and directional orientation resulting from the fabrication processes.

Since the T_{NDT} of a given steel is related to the temperature dependence of dislocation motion in the steel and of cleavage fracture mechanisms, it stands that shifts in this property as a result of cold work or, as discussed below, irradiation, must be accompanied by shifts in the ductility, which in turn can be measured as changes in the micro-hardness of the material (the value of which depends upon deformation mechanisms within the steels) or the yield strength. Consequently, while the majority of the monitoring programs for pressure vessel steels are based on measured changes in T_{NDT} by Charpy impact specimen techniques, we are now learning that correlations of these other properties may be sufficiently reliable to be used to predict shifts in T_{NDT} .

2.0 Effects of Neutron Irradiation on Pressure Vessel Steels.

When pressure vessel steels are irradiated by neutrons of sufficient energy to knock atoms out of their place in the lattice (usually > 0.1 MeV) the resultant damage to the structure of the material produces changes in yield strength, tensile strength, ductility, and shifts in the ductile-brittle transformation. At all temperatures above 0 degrees K, the displacement of the atoms by the neutron flux is countered by thermally activated diffusion processes within the steel that allow interstitial atoms or vacancies produced by the irradiation to readjust in the lattice to more comfortable positions. The nature of other impurities in the steel also affect the changes in these properties. Consequently, the shift in the T_{NDT} and changes in other properties are a function of the chemistry of the material, the irradiation temperature, the rate of irradiation, and the prior mechanical history of the material.

2.1 Role of Material Chemistry. Empirical correlations and statistical analysis of wide ranges of data show that the extent of the shift in T_{NDT} , or changes in other physical properties, can be related primarily to the concentration in the steel of copper, phosphorus, and nickel, with lesser effects of manganese, antimony, and tin. The principal effect is that of the copper (Cu) content of the material. The effects of phosphorus (P) are lesser in U.S. than Soviet steels, because of careful control to limit P to $< .012$ percent; in the older (A grades) Soviet steels the P content may range up to 0.03 percent. The effect of P is most significant when Cu is low (< 0.15 percent). At high Cu levels (approximately 0.3 percent) no additional effect from P is observed, and the P is found as a Cu - P precipitated compound, in U.S. steels. The effect of nickel is synergistic to that of copper, that is in a copper-free material there is little effect on mechanical properties under radiation due to the presence of small amounts of nickel in the steel. However, when copper is also present, nickel augments the effect of the copper. Various modeling studies in the U.S. and in the Soviet Union to predict the irradiation behavior of steel from its chemical composition are discussed below in Section 3. Most of the attention of the researchers has been on the role of copper; however, shifts in T_{NDT} due to radiation also occur in copper-free steels and even pure iron, so that copper should be considered as a major, but not the sole contributor. The copper containing precipitates found in irradiated steels are not pure Cu, but contain significant amounts of Fe, Mn, and Ni. In Soviet steels containing < 0.15 percent Cu, the major contributor to embrittlement appears to be P.

2.2 Role of Irradiation Temperature. As noted above, the radiation damage produced by fast neutrons knocking atoms from their position in the lattice structure of the steel is accompanied at all times by thermally activated diffusion related processes, in which these knocked-on atoms migrate to more stable positions in the lattice. Consequently, the higher the irradiation temperature over the range zero to 350 degrees Centigrade, the lesser the effect of neutron irradiation will be on shifts in hardness and ductile-brittle transformation. Thus, the effects of neutron flux on hardness of the material are greatest in low temperature research reactors, intermediate in some boiling water reactors, and least in pressurized water reactors operating over 300 degrees Centigrade. This point is illustrated using data from test reactor irradiations and PWR monitoring coupons as shown in the accompanying figure 1. Since PWRs, and especially VVERs, have narrower water gaps between the fuel and the pressure vessel, the fast neutron flux reaching the pressure vessel is considerably greater in PWRs than in BWRs, and therefore, the shifts in ductility and T_{NDT} per given period of time are greater in these reactors.

2.3 Role of Dose Rate (flux). Most studies of radiation effects have been done in relatively high flux research reactor facilities in order to obtain fast fluences in the ranges of interest in a reasonable period of time. Also, all operating U. S. pressurized and boiling water reactors have a coupon monitoring system, in which specimens cut from the same heat of material used in the pressure vessel or welds from the same heat and procedure are irradiated in the water space between the fuel and the vessel wall. Usually these are located so as to receive several (up to 20) times the flux of the pressure vessel, so that the behavior of the actual steel and weld metal used in the vessel can be monitored and changes observed from the coupons well in advance of the time they would be observed in the reactor vessel itself. However, recent observations have shown that irradiation at a lower flux to a given fluence can increase the effect on mechanical properties. This result implies that the pressure vessels themselves may be undergoing greater irradiation damage than the surveillance coupons at equivalent fluences. Figure 2 shows the effects of this dose rate phenomenon on mechanical properties of the pressure vessel steel in the HFIR at Oak Ridge National Laboratory. As can be seen, samples removed from the vessel surveillance program show considerably more damage than samples irradiated to the same fluence in a few weeks in the Oak Ridge Reactor (ORR). Recent observations in Germany on samples removed from the decommissioned Gundremmingen (BWR) reactor vessel have shown greater increases in hardness on the vessel samples themselves than would have been predicted from the surveillance coupons, as well as orientation effects that were not anticipated. While this is still a preliminary observation, its impact on interpretation of surveillance specimens is obvious. The dose rate phenomenon has not been discussed or modeled in any detail in the literature. One might suggest that at the higher fluxes the rearrangement of the lattice structure following displacement of neutrons is accelerated by subsequent bombardments or the thermal effects of these bombardments.

2.4 Interactions Between Irradiation and Mechanical Factors that Influence T_{NDT} . A number of authors have noted that there are direct correlations between changes in the nil ductility temperature and changes in properties such as yield strength and micro-hardness of the irradiated material. Figure 3 illustrates some of these effects. Further quantification of these correlations may permit irradiating much smaller surveillance specimens to utilize micro-hardness measurements rather than the relatively large Charpy impact specimens irradiated at the present time. Recognizing that both of these effects (the shift in ductile-brittle transformation and the increase in hardness) are brought about by the effects of neutrons on the dislocation structure of the material and the ability of the material to deform under stress, the reasons for the correlation become apparent. Similar shifts in hardness and ductility are brought about by mechanical cold work on the pressure vessel or any other steel. It has been shown conclusively, however, both in the U.S. and in the Soviet Union, that subsequent irradiation of cold worked material does not produce as large a shift in mechanical properties as one might predict from simply adding the effects of the individual phenomena. Published data show that the irradiation-induced shifts in T_{NDT} of a 10 percent cold worked steel are less than the shift in an annealed specimen of the same material. However, the absolute value of T_{NDT} for the cold worked material would still be greater than that of the annealed material given the same fluence. For most cases, pressure vessels are specified to be heat treated following welding at approximately 600-650 degrees Centigrade to relieve residual stresses produced by the forming and welding processes. This heat treatment would also dissolve the copper and minimize damage from cold work, so that

the contribution from any cold work that occurred during forming the vessel usually can be ignored in estimating radiation embrittlement, provided the heat treatment was performed properly. Nikolaev et al., have shown a significant effect of cooling rate from the heat treating temperature on the radiation induced embrittlement behavior of both steel and weld metal, which they attribute to precipitation kinetics of Cu and P: in their work, slow cooling makes the steel (15 Cr₂ NMFA) less susceptible to embrittlement. Considerable effects of orientation with respect to the plate rolling direction have been seen in the U.S. and in the Gundremmingen vessel examinations cited above.

2.5 Distinction Between Shift in T_{NDT} and Reduction in "Upper Shelf Energy (USE)". As can be seen in figure 4, the fast neutron fluence on a pressure vessel steel not only causes a shift in T_{NDT} but a reduction in the amount of energy the material can absorb without fracture at temperatures above the transition temperature (in this case from approximately 130 joules to 90 joules). Severe reductions in this upper shelf energy (USE) obviously are to be avoided since they increase the propensity for low energy fracture at temperatures well into the operating range for most LWRs. In the studies on annealing of radiation effects in pressure vessel steels, attention is given to the restoration of the original values for both T_{NDT} and the upper shelf energy, as will be discussed below. A minimum of 50 foot-pounds USE (68 joules) is allowed in the U.S. for continued operation of a pressure vessel.

3.0 Monitoring and Predicting Shifts in T_{NDT}

Because of uncertainties in predicting the irradiation effects on the pressure vessel steels, most operating LWRs with steel pressure vessels contain specimens being irradiated in the core (beltline) region of the reactor, where the fast neutron fluence is the greatest. Generally, these are somewhat closer to the core than the vessel due to requirements for insertion and removal of specimens. This also means, however, that the specimens receive a flux several times higher than the vessel wall, and shifts in the T_{NDT} measured may not be entirely conservative. In the United States subsequent pressurization temperatures are set by the Code and other requirements that the vessel not be pressurized at temperatures below $RT_{NDT} + 90$ degrees Fahrenheit (50 degrees Centigrade). This is a highly conservative requirement, since the size flaw required to initiate brittle failure at the fracture toughness, at temperatures corresponding to the 41 joule level in a CVN test, and the pressurization stresses would be a considerable fraction (.25 to .5) of the thickness of the vessel wall, oriented in the worst direction.

3.1 Sampling of the Vessel Material. Sixteen Soviet designed VVERs, the VVER-440's model V230s, have pressure vessels without stainless steel cladding on the inside. Because of concern about radiation embrittlement of these steels and interest in monitoring the reactor vessels, especially in light of the relatively high fluence received by these reactor vessels and the absence of monitoring coupons in most of these reactors, the Soviet Union, the German Democratic Republic, and Czechoslovakia have each devised protective cabins in which inspectors can enter the vessel and remotely or manually cut out small specimens from the vessel wall itself. In the laboratory, shifts in micro-hardness of this material can be correlated with shifts of T_{NDT} as described above for estimating changes in properties of the actual wall. The Soviet literature describes methods for setting safe pressure/temperature regimes based on calculated shifts (as outlined below) determined from experimental data from irradiation in various high flux research facilities. How conservative this technique is remains to be determined. The Soviets have also developed a technique for remotely taking microhardness measurements of the inner surface of these unclad vessels (see Background Appendix G, Operations and Maintenance).

3.2 Methods of Estimating Shifts in T_{NDT} from the Chemistry of Metals and Weld. As noted above, shifts in mechanical properties and T_{NDT} under neutron bombardment are a function of the chemistry of the material. These effects differ between weld metal and wrought material (probably as a result in differences in their micro-structures). A number of empirical equations have been developed to estimate these shifts. The most significant effects are those of copper and the synergism between nickel and copper with lesser effects due to phosphorus, and other impurities. Revision 1 to USNRC Regulatory Guide 1.99, which was issued in 1977, included curves for estimating the shift in RT_{NDT} as a function

of fast fluence. These curves (which were based largely upon work performed at the Naval Research Laboratory) were highly conservative, and over-predict the results shown from surveillance programs on a number of reactor vessels and welds. They are shown in addendum 1. Recent attempts to modify and update this regulatory guide have resulted in a revision 2 to the guide, which lists methods of predicting the shift in T_{NDT} as a function of copper and nickel concentration in tabular form. These are included in addendum 2. These methods still represent the outer bound of data obtained both from test reactor radiations and from the surveillance coupons being analyzed from the various operating LWRs in the United States. They also assume the contents of phosphorous and other impurities to be negligibly low. They are conservative for the data base on which they were obtained, but may not be conservative outside the ranges of Cu, Ni, P, etc., used in the original data.

French PWRs also have surveillance coupons. A series of equations for predicting the shift has been developed by the French from analysis of these and test reactor data.

$$RT_{NDT}, ^\circ C = 36 + 310 (\% Ni)^2 (\% Cu) + 361 (\% Cu - 0.08) + 2328 (\% P - 0.008) \quad (1)$$

for $F = 5 \times 10^{19} \text{ n/cm}^2$ ($E > 1 \text{ MeV}$)

Data obtained in test reactors for steels used in VVERs have been used by the Soviets to develop a formula that assumes a cube root dependence of T_{NDT} on fast fluence.

$$\Delta T_{NDT} = A_F F^{(1/3)}, ^\circ C \quad (2)$$

where F is fast fluence ($E > 0.5 \text{ MeV}$) $\times 10^{18}$ neutrons/centimeters², and A_F is the coefficient of irradiation embrittlement, defined as (for irradiation at 300 degrees Centigrade),

$$A_F^{(300)} = 700(C_{Ni} + C_{Mn} + 1.5 C_{Cu}) (C_P + 0.25 C_{Sb} + 0.25 C_{Sn}) \quad (3),$$

where C_x is the concentration of species x , in weight percent. Equations based on VVER surveillance coupons are discussed below in Section 8. In table A-2, the calculated shifts in T_{NDT} for the steels given in table 1 at a fast fluence of $5 \times 10^{19}/\text{cm}^2$ are listed, as estimated from the four methods given. It can be seen that the predicted shifts from the various methods scatter, but probably no more than the range of experimental data upon which each were based. While using the maximum allowable levels of phosphorus in the Soviet steels may have biased these calculated results slightly, the combination of higher allowable levels of Cu and P, and the high flux at the beltline in VVER-440's makes their susceptibility to radiation-induced embrittlement significantly higher than that of Western PWRs. Further, the high fluxes in these reactors will yield a significant amount of embrittlement, even though Cu and P levels may be reduced. None of these equations attempts to approximate the dose rate effect at the flux levels that prevail in LWRs. They are however all conservative in that they represent the outer-bound of the (high flux) data upon which they were based. However, U.S. data do not confirm the multiple effect of $Cu \times P$, shown in equation (3), as noted above, possibly because of the lower range of phosphorus contents of U.S. steels.

4.0 Mechanisms of Radiation Enhanced Embrittlement.

The enormously complex structure of steels has prevented any detailed mechanistic model of the radiation embrittlement phenomenon and its annealing from being developed. The best that has been done so far is the development of a number of empirical models which predict many, but not necessarily all, of the effects. Work with radiation at low temperatures of high purity iron showed that shifts in the T_{NDT} even for the high purity material occur as a result of irradiation; this indicates that fast neutron induced defects, (defect clusters, vacancy clusters, dislocation arrays, etc.) are sufficient in themselves to cause some effect. Studies using field ion microscopy of pressure vessel steels have confirmed the effect of copper in that arrays of copper-containing precipitates have been identified; therefore, let us discuss the role of copper first, recognizing, however, that this is not the whole story.

4.1 Copper Precipitation. Copper is highly soluble in gamma iron. Copper-containing scrap is often introduced in the refining process, and copper is not readily removed by the refining process itself; consequently, when the ingots are cast and then worked at high temperature (in the gamma range) some copper remains, which is randomly dispersed in solid solution in the metal. The solid solubility of copper in alpha-iron (the normal structure of the steels in the temperature range of their application) however, is quite low. It can be approximated by the equation:

$$\log_{10} (\text{wt } \% \text{Cu}) = 4.335 - 4499/T(^{\circ}\text{K}). \quad (4)$$

From this equation, the solubility of copper at 300 degrees Centigrade, a typical vessel temperature in a LWR, is approximately 3 parts per million (ppm). This is considerably less than the concentration typically found in pressure vessel steels (500-2000 ppm). However, diffusion processes at the normal operating temperatures are much too low for any significant precipitation of the copper to occur in the absence of radiation. The solubility of copper at the stress-relieving temperature is approximately 0.3 percent, so that little precipitation should occur during this treatment, although it can and does occur during the subsequent cooldown from this treatment. The various types of defects produced by the fast neutrons, however, give rise to locally stressed sites in the lattice, to which copper atoms can segregate. These precipitates are not pure Cu, but may contain significant amounts of Fe, Mn, Ni, or P. Their interaction with the dislocations and other irradiation induced defects tends to block dislocation motion in the steel and therefore increase its yield strength, hardness, and T_{NDT} . Pre-precipitation of copper during the heat-treatment (or cooldown) can significantly influence the subsequent irradiation embrittlement. While nickel is also a face centered cubic metal, it is soluble in alpha-iron up to more than 10 atomic percent at 300 to 400 degrees Centigrade. In the absence of copper, therefore, nickel does not precipitate and show a similar effect on radiation-induced hardening. However, in the presence of copper, the nickel appears to increase the rate of copper precipitation or co-precipitate and therefore increases the sensitivity of the mechanical properties to copper content in a synergistic fashion. Phosphorus is also insoluble in alpha-iron and, if trapped in the lattice, can precipitate at radiation-induced defects, giving a similar effect. In most high quality U.S. steels the phosphorus is low enough, and its variation small enough, that it appears to have no measurable effect on the changes in mechanical properties. This is not true of Soviet "AA" grade steels (see table 1), used in some newer VVERs.

As noted above, at any temperature greater than 0 degrees K, some subsequent motion of the radiation-induced defects is occurring in the metal lattice. Consequently, the observed experimental results represent the combined effects of the radiation knocking atoms out of their site in the lattice and subsequent thermally activated relocation re-precipitation processes. The neutron displacements are not expected to be thermally activated to any great extent; the temperature effect on irradiation, therefore, is largely a temperature effect on the subsequent behavior of the radiation produced defects. The dose rate effect can similarly be considered in an empirical model as an increase with flux of the interactions between the neutron displaced atoms and the thermally activated development of defect clusters, interstitials, copper precipitation, etc.; as noted above, there is no quantitative model for explaining this phenomenon.

Several excellent reviews of the models that have been proposed are cited in the Bibliography.

5.0 Annealing of Radiation Effects in Pressure Vessel Steels

As noted above, the observed effects of radiation are the sum of the displacement processes caused by the neutrons and the subsequent migration of these defects, which sum is characteristic of the radiation temperature. In principle, therefore, heating the material to a temperature greater than that at which it was irradiated is sufficient to cause some increased motion of the radiation produced defects and, therefore some restoration of the original properties of the material. Experimentally, heating the material to more than 50 degrees Centigrade above the irradiation temperature gives a partial restoration of properties. This restoration is time-dependent and temperature-dependent. Thus, the greater the temperature increment between the irradiation temperature and the annealing temperature, and the longer the period of time, the greater the restoration of the original properties. For most pressure vessel steels that have been irradiated at temperatures in the range of 300 to 325 degrees Centigrade, annealing for 1 to 4 days at 450 to 500 degrees Centigrade appears to be sufficient to restore both the upper shelf energy and T_{NDT} to approximately their original values, as can be seen in figures 5 and 6. Re-irradiation of the annealed material generally causes the same rate of increase in hardness and other properties with fluence as occurred originally. However, the rate of increase of these properties on re-irradiation appears to be greater with lower annealing temperature.

This phenomenon has been difficult to explain in terms of the present models of radiation induced defects. As noted above, 450 degrees Centigrade is not a high enough temperature to redissolve the precipitated copper; it may be high enough to allow the copper subcritical nuclei to grow and form a nucleus of a separate phase within the steel, which then interferes considerably less with the dislocation motion. Re-embrittlement upon re-irradiation has to be based on the assumption that not all the available copper precipitated the first time around, so that more is available. However, this point is by no means clear. The cumulative effects of periodic annealing and re-irradiation of pressure vessel steels have not been demonstrated in the U.S. Preliminary data show re-embrittlement phenomena to be strongly material-dependent, suggesting the specific vessel or weld materials need to be carefully monitored. Preliminary results suggest that high copper steels re-embrittle more rapidly than low copper steels.

Annealing of pressure vessels in situ presents a major engineering challenge. If the irradiation temperature is low enough, as in some of the early VVERs (265 to 285 degrees Centigrade) some recovery of properties can be obtained by heating the water up to temperatures close to the critical point (350 - 360 degrees Centigrade). To do this with liquid water would require pressures higher than those for which the systems were designed. The use of hot steam in the same temperature region has also been discussed. Heating the vessel or the affected part of it, however, to temperatures high enough for effectively complete recovery of the radiation properties (450 to 500 degrees Centigrade) requires a source of dry heat, such as electrical resistance heaters or super-critical steam. Difficulties arise from thermal stresses that might be produced in the vessel if it is heated locally or between the vessel and the piping system if the entire vessel is heated up to 450 to 500 degrees Centigrade. Regardless of the means used to achieve the requisite temperatures, the temperatures, temperature gradients, and heat up/cool down rates must be properly specified and controlled to avoid damage.

In practice it may not always be possible to cut the piping from the vessel, while performing this annealing. This is especially true of the VVER-440 piping, which must pass through small-clearance gaps in the building structure. Subsequent monitoring of the effectiveness of the annealing and re-embrittlement on the actual vessel material is essential. The Soviets could, in principle, do this with their unclad vessels and remote sampling and microhardness techniques.

There have recently been annealing experiments performed on pressure vessels at two of the VVERs at Novovoronezh. The published results, that are from Novovoronezh-3, are summarized in section 8 of this Appendix, along with annealing techniques at two other operating VVER-440's.

6.0 Field Experience and Proposals on Annealing of Pressure Vessels.

Tests in the U.S. on annealing of irradiated steel specimens have been carried sufficiently far that methods for dry 450 to 500 degrees Centigrade annealing of PWR pressure vessels have been developed by EPRI and Westinghouse. Based on this work (and on other U.S. work) an ASTM procedure has been tentatively adopted. The EPRI/Westinghouse proposed thermal annealing procedure utilizes dry localized heating to supply an annealing temperature of 850 degrees Fahrenheit (454 degrees Centigrade) for 168 hours. Heating would be applied by a resistance type heating element system designed in a manner so that only the areas requiring thermal annealing would be heated. Removal of the reactor vessel internals and all fuel assemblies would be required. The resistance heating elements would be cycled on and off to achieve the specified thermal annealing parameters. The progress of the annealing would be monitored by micro-hardness measurements on surveillance specimens of the materials and welds subjected to the same annealing cycle in the vessel. The ASTM procedure (E-509-86) does not specify an annealing temperature, but emphasizes the need for selecting a temperature based on data taken from specimens of the steel, for the degree of recovery to be achieved.

The Soviet technique involved resistance heating and is discussed in Section 8 below. The Soviet literature discusses re-irradiation effects on $15\text{Cr}_2\text{MFA}$ and $15\text{Cr}_2\text{NMFA}$ steels; results are discussed in Section 8 below. Recent work in the U.S. has shown that annealing and re-irradiation effects need to be studied or monitored on specimens of the steel and weld material of the vessel being annealed.

7.0 Anticipated Benefits on Life Extension from Annealing.

In the absence of any measures to reduce the flux on the pressure vessel subsequent to annealing, the maximum benefit anticipated from annealing would be an extension in life equivalent to the number of effective full power days of irradiation that the given vessel had received up to the point of the annealing. However, thermal stresses developed in the vessel itself or in the piping, incomplete annealing and other unknown factors that may contribute to the post-annealing shift in ductility of the steel may reduce the life extension practically achievable by this technique to approximately 50 percent of the original effective full power days. Many older PWRs in the U.S. and some of the older VVER-440's have adopted a change in fuel loading or fuel cycle designed to reduce the fast neutron flux at the vessel wall. Based on Finnish data, the core modifications to Loviisa-1 are predicted to reduce the transition temperature shift by over 50 degrees Centigrade during the 30 year anticipated life of the reactor, as shown in figure 7. This reduction brings their operation within feasibility for the 30 year period, so that annealing is not planned for Loviisa. Demonstrating the effectiveness of annealing has been a stumbling block to application of annealing in the western nations, to date. Further developments are required, including improved techniques for monitoring the effectiveness of the anneal and estimating thermal stress damage to the vessel and/or other parts of the system, before applications can be widespread.

8.0 Embrittlement of VVER Pressure Vessels.

In the preceding sections the general topics of radiation effects and annealing have been discussed. The purpose of this section is to evaluate the situation with regard to VVER pressure vessels.

The designs of VVER-440 Models V230 and V213, reactor pressure vessels have a relatively (compared to U.S. PWRs) narrow water gap between the outer rows of fuel elements and the vessel wall (27 centimeters, vs 40 centimeters for

a typical Westinghouse PWR). Table A-3 compares the lifetime fast fluences for VVER pressure vessels with those received by U.S. and German PWR pressure vessels. This narrow water gap in VVER-440's results in a relatively high (approximately 10^{11} n/cm²-sec) fast ($E > 1$ MeV) flux on the vessel wall. For the VVER-1000s, the water gap is increased to 39.3 centimeters, and the fast flux on the vessel reduced by approximately a factor of 4. It is still comparable to the highest PWR fluxes, however. Therefore, radiation embrittlement is an important element in the operation of all VVERs. Soviet steels used in the early VVERs were relatively high in phosphorous and copper -- VVER-1000 steels contain 1 percent nickel. Several remedial actions have been implemented at some plants, as discussed below. Other units, however, continue to operate with these high fluxes unmitigated.

The early VVER-440's, including all model V230 units, were designed to operate without radiation monitoring coupons. The intent was to estimate the degree of embrittlement from (relatively short term) radiation data obtained in research reactors, using equations of the type given above as (2) and (3). The builders of Loviisa required that chains of coupons to determine embrittlement be installed in the downcomer region between the core barrel and the vessel. They thus receive a neutron fluence approximately 10 to 16 times that of the vessel wall and the "core weld". For Loviisa, these were made from similar heats of 15Cr₂MFA steel to the vessels, and weld specimens were fabricated at the factory by the same welders that worked on the vessels. They are not, however, believed to be exact duplicates of the vessel and weld materials. Chains of surveillance specimens similar to those in Loviisa were subsequently inserted at Armenia, Paks and Rovno, among the VVER-440 units. In all VVER-1000 units, surveillance specimens are inserted in small containers in a rig above the core.

VVER pressure vessels are fabricated from forged rings; therefore, there is no vertical seam weld in the highest flux area. In most VVER-440's, however, one of the circumferential welds is located just below the midpoint of the fuel bundle, and therefore receives nearly the maximum fast fluence. This is referred to as the "core weld." Advances in pressure vessel fabrication technology, however, have permitted the production of wider rings, so that welds are reputed not to be in the peak flux region in most VVER-1000s and in the latest VVER-440 units, including the reactors under construction in Cuba. Drawings of VVER-1000 vessels, however, show two circumferential welds approximately 20 percent of the core height above the bottom and below the top of the fuel bundle; these receive a substantial fluence. Nikolaev has discussed the effects of radiation on weld deposits of the alloys used in VVER-1000s, at Cu and P levels in the range shown in table 1 for 15Cr₂ NMFA steel. This suggests that embrittlement of VVER-1000 vessels and their welds may be a significant concern for some of the operating units.

The results from the surveillance specimens at Loviisa-1 have shown measured values of the coefficient of embrittlement, A_F , from weld specimens have been approximately 35, as shown in table A-2. Specimens from other units, including Loviisa-2, have yielded values of A_F closer to the predicted numbers, except for Rovno 2, which measured A_F of approximately 33 on the core weld. The reasons for this wide range of measured values are not apparent from the chemistry and thermo-mechanical history of the specimens. The combination of these high coefficients of embrittlement, high fast flux, uncertainties on how well the specimens represent the actual vessel wall, and the more recent observations of a dose rate effect indicate remedial actions are required for continued safe operations of older VVER-440's. An additional uncertainty arises from possible variations in the original values of T_{NDT} for the un-irradiated vessel and core welds. The forged rings used in fabricating the VVER vessels have been shown to be less susceptible to directional effects than the rolled plates used in most U.S. vessels.

Results from surveillance coupons from a number of VVER-440 units in the Soviet Union and eastern Europe have recently been summarized as follows, in terms of the coefficient of embrittlement, A_F (equation (2)).

For the base metal, 15Cr₂MFA

$$A_F (265 \text{ degrees Centigrade}) = 1200 (P + 0.1 \text{ Cu}) - 19 \quad (5)$$

$$A_F (285 \text{ degrees Centigrade}) = 600 (P + 0.1 \text{ Cu}) - 1.5 \quad (6)$$

For the weld metal Sv - 10CrMFT

$$A_F (265 \text{ degrees Centigrade}) = 1200 (P + 0.1 \text{ Cu}) - 16 \quad (7)$$

$$A_F (285 \text{ degrees Centigrade}) = 600 (P + 0.1 \text{ Cu}) - 1.5 \quad (8)$$

where P and Cu represent weight percent phosphorus and copper. Again, the role of phosphorus in the Soviet steels is predominant.

Proposed remedial actions include reducing the flux by replacing the outermost row of fuel elements with stainless steel dummies, annealing as discussed above, and development of more embrittlement-resistant steels. All these approaches are being actively pursued. At Loviisa (and other units) the use of dummy elements has reduced the predicted transition shift of the core weld after 30 years by over 50 degrees Centigrade as shown in figure 8, and the fluence from approximately 10^{20} to $2-3 \times 10^{19}$ in 30 years. With this modification, and increases in temperature of the ECCS water and refueling water storage tanks, IVO believes Loviisa can be operated safely for up to 30 years. Annealing is being carefully considered, however, in view of the uncertainties noted above; the results of annealing Loviisa specimens that were discussed above are shown in figure 5. Further, surveillance specimens removed from Loviisa, after the flux modification, have less than the predicted reduction in embrittlement, suggesting a dose rate effect may be operable here, and that this remedial action may not be sufficient to extend the vessel life to 30^+ years, as desired.

Other VVER-440 plants have not modified their cores. (It has been reported that Bulgaria began modifying the cores at Kozloduy in 1988, and there are unsubstantiated reports of similar modifications in some other CMEA countries.) Annealing appears to be the primary remedial action under consideration. The Soviets consider annealing to be the only acceptable remedy. Specimens from the surveillance chains at Armenia-2 were irradiated to fluences up to 10^{20} n/cm² ($E > 0.5$ MeV) at 265 degrees Centigrade, annealed 144 hours at 420 degrees Centigrade, reirradiated for another 10^{20} n/cm² in the reactor, and reannealed 144 hours at 420 degrees Centigrade. The results are shown in figure 7 in terms of the shift in T_{NDT} , (T_F) as a function of exposure. It can be seen that recovery of approximately 70 percent of the shift in T_F occurred during each anneal. However, the weld material appears to have embrittled more rapidly during the second irradiation. The absence of data points on these (Soviet) curves make a more detailed interpretation difficult. A third cycle on these specimens duplicated the second. The Soviets believe the lack of complete recovery is related to the phosphorus level of the steels. These results compare well with the Finnish data at 420 degrees Centigrade, shown in figure 5. The U.S. data (figure 6) show less of a recovery in 144 hours at 427 degrees Centigrade, probably because the irradiation temperatures are somewhat higher in PWRs than in VVERs.

Amayev and Dragunov have recently provided the data shown in figure 9, which shows the relationship between percent recovery of the original impact properties as a function of the difference between the annealing and the irradiation temperatures, (ΔT) for the VVER-440 pressure vessel steel, 15Cr2MFA. In agreement with the Loviisa data (figure 5) these results show that a ΔT of at least 200°C is required to achieve >90% recovery of the unirradiated properties. They also show a scatterband of approximately 30% for any given ΔT , which they attribute to effects of variations in phosphorus levels.

Soviet calculations have shown that if a band, one meter wide, centered on the core weld in a VVER, is heated to 450 degrees Centigrade by resistance heaters, the temperature at the vessel support clamp does not exceed its normal operating temperature (250 degrees Centigrade for Novovoronezh-1) during the annealing. Maintaining heatup and

cooldown rates below 20 degrees Centigrade per hour is intended to ensure thermal stresses along the thickness will not exceed 30 to 40 MPa.

Following a test anneal at the decommissioned Novovoronezh-1, a demonstration anneal has recently been performed at Novovoronezh-3, a VVER-440 model 230 unit with an unclad vessel, that has operated since 1972. Small specimens cut from the vessel wall were used to monitor changes in micro-hardness and tensile properties. After all internals, including the core barrel were removed, resistance heaters were placed inside the (dry) vessel, and a band, centered on the core weld, one meter wide was heated to 430 degrees Centigrade for one week. (The fuel pins in the core are 2.57 meters long). A recovery of 70 percent of the embrittlement was claimed, which is entirely consistent with the Armenia and Loviisa data at 420 degrees Centigrade (figures 5 and 7), and the lower bound of the Soviet data (Figure 9) for $\Delta T = 165^\circ$. The unit is reported to have been returned to service with a normal core (no dummy elements). The basic steel in the core area was not annealed since "there was no necessity for this". Subsequent anneals were performed at Armenia-1 and Nord-1 at 460°C, using "improved techniques." Additional anneals are planned for Kola-1 and Kozloduy 1 or 3 in the near future.

To minimize the effects of the high fluxes in VVERs, there has been an effort made in the CMEA to produce a high quality steel with very low levels of the trace elements that promote embrittlement under irradiation. This has become especially important for the VVER-1000 units, that require a steel containing approximately 1 percent nickel to meet the required mechanical properties (15Cr₂NMFA). [Even though the flux on the VVER-1000 vessels is lower by approximately 4 times than that of the VVER-440's, the presence of the nickel would increase sensitivity to irradiation-induced embrittlement at the same impurity levels, in the absence of these controls.]

These low impurity steels, designated "AA" grades [i.e., 15Cr₂MFAA and 15Cr₂NMFAA] are specified to contain low amounts of Cu and P, as shown in Table A-1. Typically, the use of "AA" grades reduces the coefficient of embrittlement to 5-6 (equation (2)). Some later VVER-1000 units and the latest VVER-440 units (including the ones under construction in Cuba) are reported to have vessels of the "AA" material.

All three of these remedial actions, reducing the peak fast flux, annealing, and use of "AA" steels in newer units, have the promise of extending the safe operating life of these vessels. In view of possible unknown factors in radiation-induced embrittlement and the uncertainties listed above, the monitoring of VVER pressure vessels remains essential. The Soviets favor annealing for the older VVER-440 vessels and the use of "AA" steels as the only long-range solutions.

An additional consideration in the embrittlement of the VVER-440 pressure vessels is hydrogen embrittlement, especially at shutdown temperatures, and especially in those units with unclad vessels. The hydrogen pickup during operation has been shown to reach up to 7.5 cm³/100 grams locally during soaking at 325 degrees Centigrade in an environment typical of that in VVERs. While this hydrogen has no effect on the mechanical properties of steel at reactor operating temperatures, at 20 degrees Centigrade and 70 degrees Centigrade, the hydrogen has been shown to migrate to areas of local high stress intensity and, therefore, to have an embrittling effect on steel, and especially on the core weld. Recommended solutions (by CMEA researchers) to the problem (for VVER-1000s) include control of impurities in steel and welding rod (such as antimony, tin, arsenic, zinc, and lead), and cladding the internal surface with austenitic stainless steel. Older VVER-440's, fabricated before control of these impurities was instigated and with unclad vessels (or with flawed cladding that allows the coolant to come in contact with the pressure vessel steel), should be considered susceptible to some degree of hydrogen embrittlement. How this interacts from radiation-induced embrittlement is not certain; the effects, like those of mechanically-induced defects, may not be additive, but are probably still harmful, especially as the hydrogen is likely to concentrate in the stress fields produced by the irradiation-induced defects in the steel. One solution for those susceptible vessels would be to maintain temperatures as high as possible during shutdown. The only available Soviet data suggest that material irradiated in contact with water is no more brittle than material irradiated in sealed capsules.

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Figure 1 The Effect of Irradiation Temperature on Mechanical Properties of A533B Steel

Figure 2 The Effect of Dose Rate at $\sim 75^\circ\text{C}$ on Shift in T_{NDT} of A212B Steel

Figure 3 Effect of Neutron Fluence on Different Mechanical Properties of Steel A, A533B, Irradiated at 290°C

Figure 4 Radiation Embrittlement of the Core Weld in Loviisa-1

BASE METAL

$$Y = A + B * e^{-(t/T)}$$

$$\begin{aligned} * - 360 \text{ } ^\circ\text{C} \quad T &= 15.7 \text{ h} \\ A &= 32.7 \% \\ B &= -14.7 \% \end{aligned}$$

$$\begin{aligned} O - 420 \text{ } ^\circ\text{C} \quad T &= 42.2 \text{ h} \\ A &= 76.7 \% \\ B &= -50.5 \% \end{aligned}$$

$$\begin{aligned} + - 480 \text{ } ^\circ\text{C} \quad T &= 10.3 \text{ h} \\ A &= 85.2 \% \\ B &= -10.2 \% \end{aligned}$$

WELD

$$Y = A + B * e^{-(t/T)}$$

$$\begin{aligned} * - 360 \text{ } ^\circ\text{C} \quad T &= 55.2 \text{ h} \\ A &= 34.8 \% \\ B &= -15.2 \% \end{aligned}$$

$$\begin{aligned} O - 420 \text{ } ^\circ\text{C} \quad T &= 72.8 \text{ h} \\ A &= 79.7 \% \\ B &= -45.0 \% \end{aligned}$$

$$\begin{aligned} + - 480 \text{ } ^\circ\text{C} \quad T &= 25.0 \text{ h} \\ A &= 92.9 \% \\ B &= -18.6 \% \end{aligned}$$

Figure 5 Relative Recovery of Hardness as a Function of Annealing Time in Steel 15Cr2MFA Loviisa-1 Specimens ($T_{in} = 265^\circ \text{C}$)

Figure 6 EPRI-Westinghouse Data on the Effect of Anneal Time on Recovery of Transition Temperature in Weld EP-24

Figure 7 Diagram of Change of Critical Brittle Temperature of 15Cr2MFA Steel (a) and its weld metal (b) depending on the neutron fluence at periodical alternation of irradiation at 285°C and annealing at 420°C for 144 hours

Figure 8(a) The Position of Dummies and the Main Azimuthal Directions

Figure 8(b) Neutron Fluence in the Main Peak Directions

Figure 9 Effect of ΔT (Annealing Temperature Less Irradiation Temperature on Recovery of Unirradiated Mechanical Properties of 15 Cr2MFA Steel

Table 1 Compositions of Nuclear Pressure Vessel Steels

Steel	Use	C	Si	Mn	S	P	Cr	Ni	Mo	V	Cu†
A-508 Class II	Early U.S.	0.27*	0.15-0.40	0.5-1.0	0.015*	0.012*	0.25-0.45	0.50-1.00	0.55-0.70	0.05*	0.10*
A-533B†	Later U.S.	0.25*	0.15-0.40	1.15-1.5	0.015*	0.012*	-	0.40-0.70	0.45-0.60	0.05*	0.10*
15Cr2MFA	VVER-440	0.13-0.18	0.17-0.37	0.3-0.6	0.020*	0.020*	2.5-3.0	0.4*	0.6-0.8	0.25-0.35	0.20±0.
15Cr2MFAA	New VVER-440	0.13-0.18	0.17-0.37	0.3-0.6	0.015*	0.012*	2.5-3.0	0.4*	0.6-0.8	0.25-0.35	0.10*
15Cr2NMFA	VVER-1000	0.11-0.21	0.17-0.37	0.3-0.6	0.025*	0.025*	1.8-2.3	1.0-1.5*	0.5-0.7	0.1-0.12*	0.30
15Cr2NMFAA	New VVER-1000	0.13-0.18	0.17-0.37	0.3-0.6	0.012*	0.010*	1.8-2.3	1.0-1.5	0.5-0.7	0.10-0.12	0.10*

† Welds in A-508 and A-533B may exceed 0.30% Cu in older units.

* Maximum

† Nuclear Beltline Considerations

*AA" Grades specify <0.01 As, 0.005 Sn, 0.005 Sb. "AA" designation used for welds means <0.08 Cu, <0.012 P, <0.015 S.

Table 2 Shifts in T_{NDT} , Degrees Centigrade⁽¹⁾ for the Steels Given in Table 1

$$\phi = 5 \times 10^{19} \text{ n/cm}^2, E > 0.5 \text{ MeV}$$

Steel	RG 1.99	RG 1.99, Rev. 2	Equation (1)	Equations 2 and 3
A-508, Class II	97	52	70	51
A-533-B	97	50	62	62
15Cr2MFA	216 ⁽²⁾	130	179	84
15Cr2NMFA	161 ⁽²⁾	200	309	138
15Cr2MFAA	97	50	57	29
15Cr2NMFAA	97	52	101	48
Loviisa-1 ⁽³⁾ Core Weld	183	71.5	123.5	140

(1) Maximum values were used where listed in table 1; mid-point values were used where ranges are given

(2) Chart readings for 0.012 percent P, Soviet steels may range to 0.025 percent P.

(3) 0.17 Cu, 0.031 P, 0.17 Ni. Measured 125 degrees Centigrade shift at $4.6 \times 10^{19} \text{ n/cm}^2$ ($E > 1 \text{ MeV}$) (figure 4)

Table A-3 Radiation Doses on Pressure Vessel Materials

<u>Reactor Type</u>	<u>Flux, n/cm²/sec E > 1 MeV</u>	<u>Lifetime* Fluence, n/cm²</u>	<u>Gap mm</u>
VVER-440 (Core Weld)	1.2×10^{11}	1.2×10^{20}	270
VVER-440 max	1.5×10^{11}	1.5×10^{20}	270
VVER-1000	$3\text{-}4 \times 10^{10}$	$3\text{-}4 \times 10^{19}$	393
Westinghouse PWR	4×10^{10}	4×10^{19}	400-500
B & W - PWR	1.2×10^{10}	1.2×10^{19}	529
KWU-1300 - PWR	5×10^9	5×10^{18}	781

* VVERs 30 Effective Full Power Years
Others 32 Effective Full Power Years

Addendum 1

Curves for estimating shifts in RT_{NDT} from Regulatory Guide 1.99, Rev. 1, 1977

Figure 1A. Predicted Adjustment of Reference Temperature, "A", as a Function of Fluence and Copper Content.
For Copper and Phosphorus Contents Other Than Those Plotted, Use the Expression for "A" Given on the Figure

FLUENCE, n/cm^2 ($E > 1 MeV$)

Figure 2-A. Fluence Factor for Use in the Expression for RT
NDT

Addendum 2 Method of Estimating Shifts in RT_{NDT}

Proposed Rev. 2 to U.S. Regulatory Guide 1.99, 1988

RT_{NDT} is calculated by the following equation:

$$RT_{NDT} = (CF) (FF),$$

in which CF, the chemistry factor is estimated from tables A-Add.2-1 or A-Add.2-2, and FF, the fluence factor, is estimated from figure A-Add.2-1, or by calculation by the equation:

$$FF = f^{(0.28 - 0.10 \log f)}$$

in which f is the fast fluence ($E > 1 \text{ MeV}$) $\times 10^{-19}$.

Table A-1
Chemistry Factor for Welds, °F

Cooper, Wt-%	Nickel, Wt-%						
	0	0.20	0.40	0.60	0.80	1.00	1.20
0	20	20	20	20	20	20	20
0.01	20	20	20	20	20	20	20
0.02	21	26	27	27	27	27	27
0.03	22	35	41	41	41	41	41
0.04	24	43	54	54	54	54	54
0.05	26	49	67	68	68	68	68
0.06	29	52	77	82	82	82	82
0.07	32	55	85	95	95	95	95
0.08	36	58	90	106	108	108	108
0.09	40	61	94	115	122	122	122
0.10	44	65	97	122	133	135	135
0.11	49	68	101	130	144	148	148
0.12	52	72	103	135	153	161	161
0.13	58	76	106	139	162	172	176
0.14	61	79	109	142	168	182	188
0.15	66	84	112	146	175	191	200
0.16	70	88	115	149	178	199	211
0.17	75	92	119	151	184	207	221
0.18	79	95	122	154	187	214	230
0.19	83	100	126	157	191	220	238
0.20	88	104	129	160	194	223	245
0.21	92	108	133	164	197	229	252
0.22	97	112	137	167	200	232	257
0.23	101	117	140	169	203	236	263
0.24	105	121	144	173	206	239	268
0.25	110	126	148	176	209	243	272
0.26	113	130	151	180	212	246	276
0.27	119	134	155	184	216	249	280
0.28	122	138	160	187	218	251	284
0.29	128	142	164	191	222	254	287
0.30	131	146	167	194	225	257	290
0.31	136	151	172	198	228	260	293
0.32	140	155	175	202	231	263	296
0.33	144	160	180	205	234	266	299
0.34	149	164	184	209	238	269	302
0.35	153	168	187	212	241	272	305
0.36	158	172	191	216	245	275	308
0.37	162	177	196	220	248	278	311
0.38	166	182	200	223	250	281	314
0.39	171	185	203	227	254	285	317
0.40	175	189	207	231	257	288	320

Table A-2

Chemistry Factor for Base Metal, °F		Nickel, Wt-%					
Copper, W-%	0	0.20	0.40	0.60	0.80	1.00	1.20
0.01	20	20	20	20	20	20	20
0.02	20	20	20	20	20	20	20
0.03	20	20	20	20	20	20	20
0.04	22	26	26	26	26	26	26
0.05	25	31	31	31	31	31	31
0.06	28	37	37	37	37	37	37
0.07	31	43	44	44	44	44	44
0.08	34	48	51	51	51	51	51
0.09	37	53	58	58	58	58	58
0.10	41	58	65	65	67	67	67
0.11	45	62	72	74	74	77	77
0.12	49	67	79	83	86	86	86
0.13	53	71	85	91	96	96	96
0.14	57	75	91	100	105	106	106
0.15	61	80	99	110	115	117	117
0.16	65	84	104	118	123	125	125
0.17	69	88	110	127	132	135	135
0.18	73	92	115	134	141	144	144
0.19	78	97	120	142	150	154	154
0.20	82	102	125	149	159	164	165
0.21	86	107	129	155	167	172	174
0.22	91	112	134	161	176	181	184
0.23	95	117	138	167	184	190	194
0.24	100	121	143	172	191	199	204
0.25	104	126	148	176	199	208	214
0.26	109	130	151	180	205	216	221
0.27	114	134	155	184	211	225	230
0.28	119	138	160	187	216	233	239
0.29	124	142	164	191	221	241	248
0.30	129	146	167	194	225	249	257
0.31	134	151	172	198	228	255	266
0.32	139	155	175	202	231	260	274
0.33	144	160	180	205	234	264	282
0.34	149	164	184	209	238	268	290
0.35	153	168	187	212	241	272	298
0.36	158	173	191	216	245	275	303
0.37	162	177	196	220	248	278	308
0.38	166	182	200	223	250	281	313
0.39	171	185	203	227	254	285	317
0.40	175	189	207	231	257	288	320

ANALYSIS APPENDIX B

LOSS OF COOLANT ACCIDENT (LOCA) BLOWDOWN ANALYSES

FIGURES

1. V-230 Hot Leg Break Reactor Side Break Flow Rate
2. V-230 Hot Leg Break Reactor Side Break Flow Enthalpy
3. V-230 Hot Leg Break Reactor Side Break Flow Quality
4. V-230 Hot Leg Break Reactor Side Break Pressure
5. V-230 Hot Leg Break Steam Generator Side Break Flow Rate
6. V-230 Hot Leg Break Steam Generator Side Break Flow Enthalpy
7. V-230 Hot Leg Break Steam Generator Side Break Flow Quality
8. V-230 Hot Leg Break Steam Generator Side Break Pressure
9. V-230 Hot Leg Break Lower Plenum Average Coolant Quality
10. V-230 Hot Leg Break Core Average Coolant Quality
11. V-230 Hot Leg Break Upper Plenum Coolant Average Quality
12. V-230 Hot Leg Break Core Average Coolant Flow Rate
13. V-213 Hot Leg Break Reactor Side Break Flow Rate
14. V-213 Hot Leg Break Reactor Side Break Flow Enthalpy
15. V-213 Hot Leg Break Reactor Side Break Flow Quality
16. V-213 Hot Leg Break Reactor Side Break Pressure
17. V-213 Hot Leg Break Steam Generator Side Break Flow Rate
18. V-213 Hot Leg Break Steam Generator Side Break Flow Enthalpy
19. V-213 Hot Leg Break Steam Generator Side Break Flow Quality
20. V-213 Hot Leg Break Steam Generator Side Break Pressure
21. V-213 Hot Leg Break Lower Plenum Average Coolant Quality
22. V-213 Hot Leg Break Core Average Coolant Quality
23. V-213 Hot Leg Break Upper Plenum Coolant Quality
24. V-213 Hot Leg Break Core Average Coolant Flow Rate
25. V-320 Hot Leg Break Reactor Side Break Flow Rate
26. V-320 Hot Leg Break Reactor Side Break Flow Enthalpy
27. V-320 Hot Leg Break Reactor Side Break Flow Quality

28. V320 Hot Leg Break Reactor Side Break Pressure
29. V320 Hot Leg Break Steam Generator Side Break Flow Rate
30. V320 Hot Leg Break Steam Generator Side Break Flow Enthalpy
31. V320 Hot Leg Break Steam Generator Side Break Flow Quality
32. V320 Hot Leg Break Generator Side Break Pressure
33. V320 Hot Leg Break Lower Plenum Average Coolant Quality
34. V320 Hot Leg Break Core Average Coolant Quality
35. V320 Hot Leg Break Upper Plenum Coolant Average Quality
36. V320 Hot Leg Break Core Average Coolant Flow Rate
37. V-230 Cold Leg Break Reactor Side Break Flow Rate
38. V-230 Cold Leg Break Reactor Side Break Flow Enthalpy
39. V-230 Cold Leg Break Reactor Side Break Flow Quality
40. V-230 Cold Leg Break Reactor Side Break Pressure
41. V-230 Cold Leg Break Pump Side Break Flow Rate
42. V-230 Cold Leg Break Pump Side Break Flow Enthalpy
43. V-230 Cold Leg Break Pump Side Break Flow Quality
44. V-230 Cold Leg Break Pump Side Break Pressure
45. V-230 Cold Leg Break Lower Plenum Average Coolant Quality
46. V-230 Cold Leg Break Core Average Coolant Quality
47. V-230 Cold Leg Break Upper Plenum Coolant Average Quality
48. V-230 Cold Leg Break Core Average Coolant Flow Rate
49. V-213 Cold Leg Break Reactor Side Break Flow Rate
50. V-213 Cold Leg Break Reactor Side Break Flow Enthalpy
51. V-213 Cold Leg Break Reactor Side Break Flow Quality
52. V-213 Cold Leg Break Reactor Side Break Pressure
53. V-213 Cold Leg Break Pump Side Break Flow Rate
54. V-213 Cold Leg Break Pump Side Break Flow Enthalpy
55. V-213 Cold Leg Break Pump Side Break Flow Quality
56. V-213 Cold Leg Break Pump Side Break Pressure

- 57. V-213 Cold Leg Break Lower Plenum Average Coolant Quality
- 58. V-213 Cold Leg Break Core Average Coolant Quality
- 59. V-213 Cold Leg Break Upper Plenum Coolant Average Quality
- 60. V-213 Cold Leg Break Core Average Coolant Flow Rate
- 61. V-320 Cold Leg Break Reactor Side Break Flow Rate
- 62. V-320 Cold Leg Break Reactor Side Break Flow Enthalpy
- 63. V-320 Cold Leg Break Reactor Side Break Flow Quality
- 64. V-320 Cold Leg Break Reactor Side Break Pressure
- 65. V-320 Cold Leg Break Pump Side Break Flow Rate
- 66. V-320 Cold Leg Break Pump Side Break Flow Enthalpy
- 67. V-320 Cold Leg Break Pump Side Break Flow Quality
- 68. V-320 Cold Leg Break Pump Side Break Pressure
- 69. V-320 Cold Leg Break Lower Plenum Average Coolant Quality
- 70. V-320 Cold Leg Break Core Average Coolant Quality
- 71. V-320 Cold Leg Break Upper Plenum Coolant Average Quality
- 72. V-320 Cold Leg Break Core Average Coolant Flow Rate

ANALYSIS APPENDIX B

LOSS OF COOLANT ACCIDENT (LOCA) BLOWDOWN ANALYSES

Introduction

A series of double ended guillotine breaks of the primary coolant system pipes have been simulated in order to calculate the fluid conditions at the breaks for use in the containment loading calculations described in Analysis Appendix C, "LOCA Blowdown Response Analyses." Mass flow rate, pressure, enthalpy, and quality at the break were calculated. Transient simulation times were limited to one minute after the break. This time frame is too short to determine if core damage definitely results, but is sufficient to determine where further analyses should be done. Both hot and cold leg breaks were simulated. In general, the cold leg breaks had significantly greater break flows and had the greater possibility of difficulty in long term core coolability. In several cases it was determined that more detailed analyses were warranted to calculate the temperature of the core resulting from the postulated accident. The results of these analyses are determined in Analysis Appendix D, "Fuel/Core Temperature Analyses."

Three different models of VVERs were simulated. The VVER-440 model V230 is the earliest production model. This plant does not have a dedicated emergency core cooling system (ECCS) but does have a limited makeup capacity (100 cubic meters per hour [433 gallons per minute]) during loss of coolant accidents. The VVER-440 model V213 has an ECCS with four passive accumulator tanks, three high pressure coolant injection pumps (HPCI), and three low pressure coolant injection pumps (LPCI). A VVER-1000 model V320 was also simulated. This model, the latest production model of the VVER-1000, has an ECCS similar to that on the VVER-440 model V213. The accumulators for the VVER-1000 model V320 have the same capacity as those of the VVER-440 model V213; the HPCI and LPCI have different capacities.

The double ended guillotine rupture of a primary coolant system pipe is the design basis accident for the VVER-440 model V213 and the VVER-1000 model V320. The design basis accident for the VVER-440 model V230 is from a 10 centimeter (3.9 inch) pipe through a 3.2 centimeter (1.3 inch) orifice insert. The lines connecting the pressurizer sprays to the hot and cold legs are such lines. The primary piping system of the VVER-440 model V230 is essentially identical to the piping system of the VVER-440 model V213. One important exception is the routing of the pressurizer lines. The likelihood of a double ended rupture would seem to be approximately the same for a VVER-440 model V230 as for a VVER-440 model V213. Therefore, the double ended pipe rupture was also simulated for the VVER-440 model V230. Because of the similarities of the primary coolant systems, this simulation also provided information about the effectiveness of the VVER-440 model V213 ECCS.

Modeling

The six large break LOCAs were simulated using the RETRAN-02 computer code. Computer input models for the VVER-440 model V230, VVER-440 model V213, and VVER-440 model V230 were constructed and tested. At the time these accidents were simulated, the CAD/CAM views of the model V230 and VVER-440 model V213 were not available. System volumes and elevations were estimated from diagrams shown in DOE/NE-0084. System and plant component performance data were also taken from this document. To the extent possible, the models were constructed on a best estimate basis.

The RETRAN Computer Code

The Electric Power Research Institute (EPRI) sponsored code, RETRAN, was used to calculate the fluid conditions at the break for the six large break loss of coolant accidents. The RETRAN code has been used extensively since 1978 and is considered to be generally applicable to large and small break loss of coolant accidents, one dimensional pressurized water reactor transients, and anticipated transients without scram. Features of the RETRAN code used to model VVER reactors are discussed here. Full details on the RETRAN code are contained in "RETRAN-02--A Program for Transient Thermal-Hydraulic Analysis of Complex Fluid Flow Systems", EPRI NP-1850-CCMA, 1984.

RETRAN uses a one-dimensional, homogeneous equilibrium mixture thermal-hydraulic model for the reactor cooling system. The pressurizer was modeled using the nonequilibrium option. The heat transfer library included two-phase natural convection heat transfer correlations. Point kinetics was used to model the reactor neutronics for these preliminary analyses (a one-dimensional neutronic model is available). The reactor shutdown system was assumed to function successfully with a second delay during the transient. The decay heat curves are consistent with those in Background Appendix F, "Decay Heat Generation in VVERs."

The model was limited to the main components of the primary coolant system. The pressurizer was modeled using the two region nonequilibrium model. Although the output of the sprays and the capacity of the pressurizer heaters were known, the control system logic for them was not known, therefore these were not modeled. The core region was modeled as a single channel, single pin. The pump models used are discussed in the individual model sections below. The isolation valves present in the VVER-440 model V230 and VVER-440 model V213 were modeled, but these were not assumed to close during the transients. The horizontal steam generators were modeled as heat exchangers with the secondary side temperature flow rate specified.

The VVER-440 model V213s and VVER-1000 model V320s have emergency core cooling systems which include passive accumulators. These accumulators are large tanks of water pressurized by nitrogen. Proper modeling of these accumulators requires nonequilibrium thermal hydraulic models not available in the version of RETRAN used here. Therefore, it was decided to model accumulator performance as a boundary condition based on an accumulator discharge curve obtained from Soviet analyses.

The emergency core cooling systems were activated on plant protection system pressure setpoints. No delay was assumed for the accumulators. The high pressure coolant injection (HPCIs) pumps and low pressure coolant injection (LPCIs) pumps have a Soviet specification that they shall activate not later than 50 seconds after the activation setpoint is reached. In keeping with the best estimate modeling used here, a 10 second delay time after the activation setpoint was reached was used.

The break was simulated by the opening and closing of valves. Opening and closing time was 0.2 seconds. There was no interface area between the two pipe breaks. Three critical flow models are available for use in RETRAN. The isenthalpic expansion model was used for saturated conditions; the Henry-Fauske model was used for subcooled conditions. The isenthalpic model generally yields lower mass flux values than the Henry-Fauske model. A discharge coefficient of .85 was used at the break locations.

VVER-440 Model V230

The VVER-440 model V230 has two significant features that are different from the VVER-440 model V213 and the VVER-1000 Model V320: 1) canned, inertialess main primary pumps; 2) a makeup pump of limited capacity (433 gallons per minute) but does not have an emergency core cooling system. The inertialess pumps were

modeled using the standard RETRAN pump curves, but with extremely low inertias. The coastdown time was determined by tripping the pumps with no break. The coastdown time of 3 seconds was found to be in agreement with values stated in the literature. The makeup pump was modeled using the given flow rate, but the fluid enthalpy was not known nor the actuation trip. The Model VVER-440 model V213 values were used. The makeup flow was assumed to be split so that half entered the reactor vessel through the downcomer and half through the upper plenum.

VVER-440 Model V213

The VVER-440 Model V213 is identical to the VVER-440 Model V230 except for the pump model and an emergency core cooling system. The standard centrifugal pump model in RETRAN was used. Two of the accumulators were modeled as injecting into the downcomer; two into the upper plenum. The high pressure injection was into the cold legs ahead of the pumps. Low pressure injection was into the cold legs and into the accumulator injection points.

VVER-1000 Model V320

The piping lengths for the VVER-1000 model V320 were estimated from the known mass of the primary piping system. Elevations were obtained from diagrams shown in DOE/NE-0084. The capacities of the components of the emergency core cooling system were for the VVER-1000 model V187 prototype plant. The core flow areas, heat transfer area, and volume were for a VVER-1000 model V320.

Hot Leg Break

The hot leg break location was selected as being just before the entrance to the steam generator manifold. This location for the VVER-440 model V230 and the VVER-440 model V213 was in the smaller steam generator accident localization compartment. Therefore, it was assumed that this location would lead to greater challenges to the accident mitigation systems. Although this location is in the disconnectable portion of a main circulation loop, the isolation gate valves were assumed to remain open throughout the transient. This is a reasonable assumption since the closing time for these manually operated valves is approximately 90 seconds under normal conditions. The break was assumed to be in the loop whose hot leg connects to the pressurizer.

VVER-440 Model V230

The VVER-440 model V230s do not have an emergency core cooling system. The blowdown response from this model is determined solely by the blowdown model chosen, the break size, discharge coefficient, and system thermal hydraulic state. The fluid mass flow rate, fluid enthalpy, fluid quality, and break pressure at the reactor side of the break are shown in figures 1 - 4. The corresponding values for the steam generator side are shown in figures 5 - 8. The flow from the steam generator side is much smoother than that from the reactor side, with lower enthalpy values and qualities. Reactor side pressures are higher than the steam generator side pressures throughout the simulation time.

The single channel, single pin model used in this analysis is not detailed enough to accurately predict core damage over the short time simulated. However, by looking at the fluid conditions within the vessel it is possible to determine if further analysis is warranted. Figures 9 - 11 show the average quality in the lower plenum, reactor core, and upper plenum, respectively. In the first 10 seconds of the transient, the fluid quality within the vessel remains relatively low. However, after that, the quality in the core rapidly increases, approaching one near 60 seconds. The average core flow rate (figure 12) is approximately zero at this time. Therefore, it is likely

that a core melt accident will result unless additional coolant is introduced into the system. The emergency makeup pump has a capacity of 100 cubic meters per hour except that the (433 gallons per minute).

VVER-440 Model V213

The VVER-440 model V213 primary coolant system and thermal hydraulic operating state is nearly identical to that of the VVER-440 model V230, except that the VVER-440 model V213 does have an emergency core cooling system. Therefore, until the actuation of the various components of the emergency core cooling system, the VVER-440 model V213 blowdown is very similar to the VVER-440 model V230 blowdown. The reactor side break flow parameters are shown in figures 13 - 16; the steam generator side break parameters in figures 17 - 20. The high pressure coolant system actuates at approximately 11 seconds after a 10 second delay. The effect of this system on the break flow conditions is small but noticeable. The passive accumulators start to inject at approximately 16 seconds and continue to discharge for approximately 15 seconds. Their effect on the reactor side break flow conditions is evident. The increase in reactor side break flow is caused by the code switching from the saturated isenthalpic blowdown model to the subcooled extended Henry-Fauske model, which has a much higher critical flow rate. As the system continues to decompress, the fluid conditions reach saturation again, causing the blowdown model to switch back and forth between the two for a short period around 20 seconds, before the subcooling is such that the extended Henry-Fauske model is used continuously from about 23 seconds. The steam generator side break flow is modeled using the saturated isenthalpic model throughout the transient.

Because of the emergency core cooling system, the vessel qualities (figures 21 - 23) remain low throughout the transient. The figures indicate, however, that the high pressure injection system alone may be insufficient to prevent substantial voiding in the core as the quality continues to increase until the accumulator injection starts. The core flow rate (figure 24) is, as might be expected with accumulator injection into both the downcomer and upper plenum, highly oscillatory. In general, the flow is positive. The low pressure injection system did not activate over the time period simulated.

VVER-1000 Model V320

VVER-1000 model V320s operates at a higher pressure and have larger diameter primary coolant pipes than the VVER-440 model V213. Therefore, the break flow rates and resulting decompression rate are larger than the VVER-440 model V213 (figures 25 - 32). The VVER-1000 model V320 has an emergency core cooling system similar to the VVER-440 model V213. However, because of the increased decompression rate and with the same 10 second delay on the high pressure injection pumps, the passive accumulators start to inject at 8 seconds or about 2 seconds before the high pressure injection pumps. Additionally, the accumulators are the same size for the VVER-1000 model V320 as the VVER-440 model V213. The VVER-1000 model V320 has a larger primary system volume and higher core power density however, and therefore the effectiveness of the accumulators is lessened compared to the VVER-440 model V213. This can be seen in figure 25 where the increased break flow rate caused by the switch to the subcooled extended Henry-Fauske model is later in time and of shorter duration than was the case in the VVER-440 model V213.

This decreased accumulator effectiveness is also evident in the vessel average qualities (figures 33 - 35), which show higher values (although they are still low) than for the VVER-440 model V213. As was the case for the VVER-440 model V213, the core flow oscillates during the transient, but is generally positive throughout (figure 36) Low pressure coolant injection starts at approximately 30 seconds.

Cold Leg Breaks

The cold leg break was assumed to occur in the loop connected to the pressurizer between the pump and the reactor vessel. For the VVER-440 model V230 and the VVER-440 model V213 this is in the nondisconnectable portion of the loop. The isolation valves, which would have little effect in this transient, are assumed to remain open. In general, the cold leg breaks are more severe than the hot leg breaks in terms of the magnitude of the break flows. Additionally, the cold leg breaks lead to quicker and more severe voiding of the reactor core.

VVER-440 Model V230

The fluid conditions at the break are shown in figures 37 - 44. The reactor side break flow is nearly twice that obtained in the hot leg simulation. Break flow enthalpies and qualities, although lower initially, quickly exceed the corresponding values for the hot leg case. The pump side break fluid flow rate is approximately the same as that obtained in the hot leg simulation; the quality at the end of the simulation is much higher.

Substantial voiding occurs within the vessel quickly (figures 45 - 47). The average quality in the core reaches unity within 10 seconds after the initiation of the break. The lower plenum quality also reaches unity at about 20 seconds. The flowrate through the core is nearly zero through most of the transient. Since the emergency makeup pump, which starts injection into the upper plenum and downcomer at approximately 55 seconds, provides only 433 gallons per minute of coolant, long term cooling of the core is doubtful.

VVER-440 Model V213

As was the case for hot leg breaks, the fluid conditions at the break (figures 48 - 56) are nearly identical to those obtained for the VVER-440 model V230 until the emergency core cooling systems activate. In this case, since the decompression rate is much greater than for the hot leg case, the passive accumulators (which were modeled with zero time delay) start injection into the core before the high pressure injection system (which was modeled with a 10 second time delay). Over the short time simulated, the effect of the subcooled extended Henry-Fauske critical flow model on the reactor side break is evident.

The vessel coolant qualities (figures 57 - 59) remain much lower than their respective values for the VVER-440 Model V230 cold leg break. Over this time scale, the average core flow rate (figure 60) is extremely oscillatory in nature.

VVER-1000 Model V320

The reactor side break flow rates are extremely large for the VVER-1000 model V320 cold leg break. The fluid conditions (figures 61 - 68) are such that the reactor primary system decompresses in approximately 15 seconds. The passive accumulators start injection at nine seconds, followed by the high pressure coolant injection pumps at 10 seconds, and the low pressure coolant injection pumps at 24 seconds. The increased flow rate in the reactor side break flow rate at 15 seconds is caused by the switch to the subcooled extended Henry-Fauske critical flow model.

The vessel average qualities (figures 69 - 71) indicate that voiding of the primary coolant in the core region occurs approximately two seconds after the start of the blowdown. This behavior is consistent with that obtained from simulations of double ended cold leg pipe breaks in large U.S. design pressurized water reactors. The accumulator injection at approximately 8 seconds starts to reduce the core average quality. The average core quality oscillates but decreases until the accumulators shut off at approximately 23 seconds. The average core

quality then begins to increase until approximately 50 seconds. At this time, a small positive flow rate (figure 72) develops in the core region decreasing the core quality for a short period of time. The accuracy of the homogeneous equilibrium flow model used in these simulations under these conditions is questionable.

Figure 1 V-230 Hot Leg Break Reactor Side Break Flow Rate

Figure 2 V-230 Hot Leg Break Side Break Flow Enthalpy

Figure 3 V-230 Hot Leg Break Reactor Side Break Flow Quality

Figure 4 V-230 Hot Leg Break Reactor Side Break Pressure

Figure 5 V-230 Hot Leg Break Steam Generator Side Break Flow Rate

Figure 6 V-230 Hot Leg Break Steam Generator Side Break Flow Enthalpy

Figure 7 V-230 Hot Leg Break Steam Generator Side Break Flow Quality

Figure 8 V-230 Hot Leg Break Steam Generator Side Break Pressure

Figure 9 V-230 Hot Leg Break Lower Plenum Average Coolant Quality

Figure 10 V-230 Hot Leg Break Core Average Coolant Quality

V-230 HOT LEG BREAK

Figure 11 V-230 Hot Leg Break Upper Plenum Coolant Average Quality

Figure 12 V-230 Hot Leg Break Core Average Coolant Flow Rate

Figure 13 V-213 Hot Leg Break Reactor Side Break Flow Rate

Figure 14 V-213 Hot Leg Break Reactor Side Break Flow Enthalpy

Figure 15 V-213 Hot Leg Break Reactor Side Break Flow Quality

Figure 16 V-213 Hot Leg Break Reactor Side Break Pressure

Figure 17 V-213 Hot Leg Break Steam Generator Side Break Flow Rate

Figure 18 V-213 Hot Leg Break Steam Generator Side Break Flow Enthalpy

Figure 19 V-213 Hot Leg Break Steam Generator Side Break Flow Quality

Figure 20 V-213 Hot Leg Break Steam Generator Side Break Pressure

Figure 21 V-213 Hot Leg Break Lower Plenum Average Coolant Quality

Figure 22 V-213 Hot Leg Break Core Average Coolant Quality

Figure 23 V-213 Hot Leg Break Upper Plenum Coolant Quality

Figure 24 V-213 Hot Leg Break Core Average Coolant Flow Rate

Figure 25 V320 Hot Leg Break Reactor Side Break Flow Rate

Figure 26 V320 Hot Leg Break Reactor Side Break Flow Enthalpy

Figure 27 V320 Hot Leg Break Reactor Side Break Flow Quality

Figure 28 V320 Hot Leg Break Reactor Side Break Pressure

Figure 29 V320 Hot Leg Break Steam Generator Side Break Flow Rate

Figure 30 V320 Hot Leg Break Steam Generator Side Break Flow Enthalpy

Figure 31 V320 Hot Leg Break Steam Generator Side Break Flow Quality

Figure 32 V320 Hot Leg Break Generator Side Break Pressure

Figure 33 V320 Hot Leg Break Lower Plenum Average Coolant Quality

V-320 HOT LEG BREAK

Figure 34 V320 Hot Leg Break Core Average Coolant Quality

Figure 35 V320 Hot Leg Break Upper Plenum Coolant Average Quality

Figure 36 V320 Hot Leg Break Core Average Coolant Flow Rate

Figure 37 V-230 Cold Leg Break Reactor Side Break Flow Rate

Figure 38 V-230 Cold Leg Break Reactor Side Break Flow Enthalpy

Figure 39 V-230 Cold Leg Break Reactor Side Break Flow Quality

Figure 40 V-230 Cold Leg Break Reactor Side Break Pressure

Figure 41 V-230 Cold Leg Break Pump Side Break Flow Rate

Figure 42 V-230 Cold Leg Break Pump Side Break Flow Enthalpy

Figure 43 V-230 Cold Leg Break Pump Side Break Flow Quality

Figure 44 V-230 Cold Leg Break Pump Side Break Pressure

Figure 45 V-230 Cold Leg Break Lower Plenum Average Coolant Quality

V-230 COLD LEG BREAK

Figure 46 V-230 Cold Leg Break Core Average Coolant Quality

Figure 47 V-230 Cold Leg Break Upper Plenum Coolant Average Quality

Figure 48 V-230 Cold Leg Break Core Average Coolant Flow Rate

Figure 49 V-213 Cold Leg Break Reactor Side Break Flow Rate

Figure 50 V-213 Cold Leg Break Reactor Side Break Flow Enthalpy

Figure 51 V-213 Cold Leg Break Reactor Side Break Flow Quality

Figure 52 V-213 Cold Leg Break Reactor Side Break Pressure

Figure 53 V-213 Cold Leg Break Pump Side Break Flow Rate

Figure 54 V-213 Cold Leg Break Pump Side Break Flow Enthalpy

Figure 55 V-213 Cold Leg Break Pump Side Break Flow Quality

Figure 56 V-213 Cold Leg Break Pump Side Break Pressure

Figure 57 V-213 Cold Leg Break Lower Plenum Average Coolant Quality

Figure 58 V-213 Cold Leg Break Core Average Coolant Quality

Figure 59 V-213 Cold Leg Break Upper Plenum Coolant Average Quality

Figure 60 V-213 Cold Leg Break Core Average Coolant Flow Rate

Figure 61 V-320 Cold Leg Break Reactor Side Break Flow Rate

Figure 62 V-320 Cold Leg Break Reactor Side Break Flow Enthalpy

Figure 63 V-320 Cold Leg Break Reactor Side Break Flow Quality

Figure 64 V-320 Cold Leg Break Reactor Side Break Pressure

Figure 65 V-320 Cold Leg Break Pump Side Break Flow Rate

Figure 66 V-320 Cold Leg Break Pump Side Break Flow Enthalpy

Figure 67 V-320 Cold Leg Break Pump Side Break Flow Quality

Figure 68 V-320 Cold Leg Break Pump Side Break Pressure

Figure 69 V-320 Cold Leg Break Lower Plenum Average Coolant Quality

V-320 COLD LEG BREAK

Figure 70 V-320 Cold Leg Break Core Average Coolant Quality

Figure 71 V-320 Cold Leg Break Upper Plenum Coolant Average Quality

Figure 72 V-320 Cold Leg Break Core Average Coolant Flow Rate

ANALYSIS APPENDIX C
LOCA BLOWDOWN RESPONSE ANALYSES

TABLE OF CONTENTS

	Page
Introduction	C-1
Pipe Break Accidents	C-2
VVER-440 Model V230	C-2
VVER-440 Model V213	C-4
VVER-1000 Model V320	C-8
Steam Generator Manifold Failure	C-9
Reactor Vessel Failure	C-11
VVER-440 Model V230	C-11
VVER-1000 Model V320	C-15
Control Rod Ejection-Induced Failure in Vessel Upper Head Region	C-17
References	C-18

FIGURES

1. Pressure Loading of VVER-440 Model V230 Accident Localization Volume Following Primary Coolant Outlet Pipe Break for Case of Venting Through Nine Vent Valves
2. Temperature Loading of VVER-440 Model V230 Accident Localization Volume Following Primary Coolant Outlet Pipe Break for Case of Venting Through Nine Vent Valves
3. Pressure Loading of VVER-440 Model V230 Accident Localization Volume Following Primary Coolant Outlet Pipe Break for Case of Venting Through Five Vent Valves
4. Temperature Loading of VVER-440 Model V230 Accident Localization Volume Following Primary Coolant Outlet Pipe Break for Case of Venting Through Five Vent Valves
5. Pressure Loading of VVER-440 Model V230 Accident Localization Volume for Primary Coolant Outlet Pipe Break Without Venting Through Vent Valves
6. Temperature Loading of VVER-440 Model V230 Accident Localization Volume for Primary Coolant Outlet Pipe Break Without Venting Through Vent Valves
7. Pressure Loading of VVER-440 Model V230 Accident Localization Volume Following 20 Centimeter Diameter Break With Unidirectional Flow for Case of Vent Valve Discharge Coefficient Equal to 0.90
8. Temperature Loading of VVER-440 Model V230 Accident Localization Volume Following 20 Centimeter diameter Break With Unidirectional Flow for Case of Vent Valve Discharge Coefficient Equal to 0.90
9. Pressure Loading of VVER-440 Model V230 Accident Localization Volume Following 20 Centimeter Diameter Break With Unidirectional Flow for Case of Vent Valve Discharge Coefficient Equal to 0.62
10. Temperature Loading of VVER-440 Model V230 Accident Localization Volume Following 20 Centimeter Diameter Break With Unidirectional Flow for Case of Vent Valve Discharge Coefficient Equal to 0.62
11. Pressure Loading of VVER-440 Model V213 Accident Localization Volume Following Primary Coolant Outlet Pipe Break
12. Temperature Loading of VVER-440 Model V213 Accident Localization Volume Following Primary Coolant Outlet Pipe Break
13. Pressure Loading of VVER-440 Model V213 Accident Localization Volume Following Primary Coolant Outlet Pipe Break
14. Temperature Loading of VVER-440 Model V213 Accident Localization Volume Following Primary Coolant Outlet Pipe Break
15. Pressure Loading of VVER-440 Model V213 Accident Localization Volume Following Primary Coolant Outlet Pipe Break for Case of Water Droplet Sprays Inoperative
16. Temperature Loading of VVER-440 Model V213 Accident Localization Volume Following Primary Coolant Outlet Pipe Break for Case of Water Droplet Sprays Inoperative

17. **Pressure Loading of VVER-440 Model V213 Accident Localization Volume Following Primary Coolant Outlet Pipe Break for Case of Water Droplet Sprays Failing to Cycle Off**
18. **Temperature Loading of VVER-440 Model V213 Accident Localization Volume Following Primary Coolant Outlet Pipe Break for Case of Water Droplet Sprays Failing to Cycle Off**
19. **Pressure Loading of VVER-440 Model V213 Accident Localization Volume Following Primary Coolant Outlet Pipe Break for Case of High Emergency Core Cooling Water Temperature**
20. **Temperature Loading of VVER-440 Model V213 Accident Localization Volume Following Primary Coolant Outlet Pipe Break for Case of High Emergency Core Cooling Water Temperature**
21. **Pressure Loading of VVER-440 Model V213 Accident Localization Volume Following Primary Coolant Outlet Pipe Break for Case of High Emergency Core Cooling Water Temperature**
22. **Temperature Loading of VVER-440 Model V213 Accident Localization Volume Following Primary Coolant Outlet Pipe Break for Case of High Emergency Core Cooling Water Temperature**
23. **Pressure Loading of VVER-440 Model V213 Accident Localization Volume Following Primary Coolant Outlet Pipe Break for Case of High Emergency Core Cooling Water Temperature and Water Droplet Sprays Inoperative**
24. **Temperature Loading of VVER-440 Model V213 Accident Localization Volume Following Primary Coolant Outlet Pipe Break for Case of High Emergency Core Cooling Water Temperature and Water Droplet Sprays Inoperative**
25. **Pressure Loading of VVER-440 Model V213 Accident Localization Volume Following Primary Coolant Outlet Pipe Break for Case of High Emergency Core Cooling Water Temperature and Failure of Water Droplet Sprays to Cycle Off**
26. **Temperature Loading of VVER-440 Model V213 Accident Localization Volume Following Primary Coolant Outlet Pipe Break for Case of High Emergency Core Cooling Water Temperature and Failure of Water Droplet Sprays to Cycle Off**
27. **Pressure Loading of VVER-440 Model V213 Accident Localization Volume Following Primary Coolant Inlet Pipe Break**
28. **Temperature Loading of VVER-440 Model V213 Accident Localization Volume Following Primary Coolant Inlet Pipe Break**
29. **Pressure Loading of VVER-440 Model V213 Accident Localization Volume Following Primary Coolant Outlet Pipe Break for Case of Reduced Check Valve Diameter**
30. **Temperature Loading of VVER-440 Model V213 Accident Localization Volume Following Primary Coolant Outlet Pipe Break for Case of Reduced Check Valve Diameter**
31. **Pressure Loading of VVER-440 Model V213 Accident Localization Volume Following Primary Coolant Outlet Pipe Break for Case of No Condensation in Bubbler-Condenser Trays**
32. **Temperature Loading of VVER-440 Model V213 Accident Localization Volume Following Primary Coolant Outlet Pipe Break for Case of No Condensation in Bubbler-Condenser Trays**

33. Pressure Loading of VVER-440 Model V213 Accident Localization Volume Following Primary Coolant Outlet Pipe Break for Case of Reduced Accident Localization Volume
34. Temperature Loading of VVER-440 Model V213 Accident Localization Volume Following Primary Coolant Outlet Pipe Break for Case of Reduced Accident Localization Volume
35. Pressure Loading of VVER-440 Model V213 Accident Localization Volume Following Primary Coolant Outlet Pipe Break for Case of Reduced Water Droplet Spray Flowrate Equal to 210 kilograms per second
36. Temperature Loading of VVER-440 Model V213 Accident Localization Volume Following Primary Coolant Outlet Pipe Break for Case of Reduced Water Droplet Spray Flowrate Equal to 210 kilograms per second
37. Pressure Loading of VVER-1000 Model V320 Containment Following Primary Coolant Outlet Pipe Break
38. Temperature Loading of VVER-1000 Model V320 Containment Following Primary Coolant Outlet Pipe Break
39. Pressure Loading of VVER-1000 Model V320 Containment Following Primary Coolant Outlet Pipe Break
40. Temperature Loading of VVER-1000 Model V320 Containment Following Primary Coolant Outlet Pipe Break
41. Pressure Loading of VVER-1000 Model V320 Containment Following Primary Coolant Outlet Pipe Break for Case of No Condensation Upon Structure and Equipment
42. Temperature Loading of VVER-1000 Model V320 Containment Following Primary Coolant Outlet Pipe Break for Case of No Condensation Upon Structure and Equipment
43. Pressure Loading of VVER-1000 Model V320 Containment Following Primary Coolant Inlet Pipe Break
44. Temperature Loading of VVER-1000 Model V320 Containment Following Primary Coolant Inlet Pipe Break
45. Pressure Loading of VVER-440 Model V213 Steam Generator Shell Following Rupture of Internal Hot Leg Manifold for Case of Venting of Pure Steam Through Main Steamline
46. Pressure Loading of VVER-440 Model V213 Steam Generator Shell Following Rupture of Internal Hot Leg Manifold for Case of Discharge of Pure Liquid Through Main Steamline
47. Pressure Loading of VVER-440 Model V213 Accident Localization Volume Following Rupture of Internal Hot Leg Manifold and Breaching of Steam Generator Shell
48. Temperature Loading of VVER-440 Model V213 Accident Localization Volume Following Rupture of Internal Hot Leg Manifold and Breaching of Steam Generator Shell
49. Pressure Loading of VVER-440 Model V213 Accident Localization Volume Following Rupture of Internal Hot Leg Manifold and Breaching of Steam Generator Shell for Case of No Condensation in Bubbler-Condenser Trays
50. Temperature Loading of VVER-440 Model V213 Accident Localization Volume Following Rupture of Internal Hot Leg Manifold and Breaching of Steam Generator Shell for Case of No Condensation in Bubbler-Condenser Trays

51. Pressure Loading of VVER-440 Model V230 Accident Localization Volume Following Failure of Reactor Pressure Vessel
52. Upward Force Loading Upon VVER-440 Model V230 Reactor Pressure Vessel Following Failure of Reactor Pressure Vessel
53. Pressure Loading of VVER-1000 Model V320 Containment Following Failure of Reactor Pressure Vessel
54. Upward Force Loading Upon VVER-1000 Model V320 Reactor Pressure Vessel Following Failure of Reactor Pressure Vessel
55. Detail of Control Rod Driveline Assembly Penetration Through Upper Head of VVER-440 Reactor Vessel (all dimensions are in millimeters)
56. Pressure Loading of VVER-440 Model V230 Accident Localization System Following Control Drive Assembly Housing Failure
57. Pressure Loading of VVER-440 Model V230 Accident Localization Volume Following Control Rod Drive Assembly Housing Failure

ANALYSIS APPENDIX C

LOCA BLOWDOWN RESPONSE ANALYSES

Introduction

This appendix presents calculations of pressures and temperatures inside VVER structures following accident initiation events which involve failure of the primary coolant system pressure boundary. The predicted pressure loadings provide a basis for assessments of the response of the structures discussed in Analysis Appendix E. Specific initiating events for which pressure loadings are calculated include the rupture of a primary coolant pipe, failure of a steam generator internal manifold, rupture of the reactor pressure vessel in the core region, and the ejection of a control rod causing failure of the control rod drive housing.

The calculations were carried out with a multicompartment thermal hydraulic computer code developed specifically for the present analysis of the VVER buildings. The structure volume is represented in terms of an arbitrary number of compartments or subcompartments. Within each subcompartment, uniform conditions of pressure, temperature, and concentration are assumed to exist. The various compartments may be connected in an arbitrary manner. Pressure differences give rise to the flow of steam, air, and entrained water droplets between the compartments.

The prediction of intercompartment flows encompasses situations in which small pressure differences may exist between neighboring compartments characterized by low flowrates, as well as situations in which large pressure drops exist resulting in choked flow between compartments. The intercompartment flowrate incorporates an effective discharge coefficient given by the expression of Schwan¹ based upon analysis of the Battelle Frankfurt and Marviken containment loading experiments. Alternatively, the discharge coefficients for particular flowpaths may be set equal to user specified values. In some instances, strictly one-way flow may be modeled between compartments representing the presence of check valves. During the primary system blowdown phase, the steam-air mixture is assumed to contain entrained water droplets in thermal equilibrium with the gas-vapor phase. The fraction of water droplets carried over from one compartment into another is also given by an expression of Schwan¹ based upon experiment analysis. Following the blowdown phase, the water droplets are envisioned to immediately settle out upon the compartment floors.

The conditions of the two-phase mixture exiting the breach may be defined in either of two manners. The first is to specify the flowrate, specific internal enthalpy, quality, and pressure of fluid immediately upstream of each side of a pipe break from calculations performed with a primary system thermal/hydraulics code. The results of the thermal/hydraulics calculations carried out for VVERs are discussed in Analysis Appendix B. The second is an internal calculation of the primary system response treating the primary system as a single volume in which the fluid has a uniform pressure, temperature, and void fraction. In this case, the flowrate of the two-phase mixture exiting the break is calculated from the homogeneous equilibrium model for two-phase flow which may undergo choking. Given the conditions of the two-phase mixture upstream of the breach, the code calculates the masses of steam and liquid water introduced into the break compartment after the fluid undergoes flash depressurization down to the compartment ambient pressure. The work performed by the expanding two-phase mixture during the depressurization process is also determined and constitutes part of the energy input to the gas-vapor phase. To continue the calculation of steam formation rates beyond the time at which the Analysis Appendix B calculations or simple blowdown calculations are terminated, the code incorporates the decay heat generation rates discussed in Background Appendix F.

Condensation upon structure and equipment is calculated using the condensation correlations of Schauer² based upon analyses of the HDR, Marviken, and Battelle Frankfurt containment loading experiments. Different correlations

describe condensation during the blowdown phase and the period shortly thereafter when forced convection effects are important versus the subsequent post-blowdown period in which natural convection dominates the condensation transport processes. The thermal conduction resistance effects of the structure upon condensation are described in terms of simple expressions for the effective thermal resistance of the concrete walls and the steel liner on the wall inner surface. Detailed multicell calculations of the wall heatup are not performed. This approach is adequate for the timescales of one hour or less following break inception over which the calculations have been carried out given the thick walls and floors employed in the accident localization and containment structures.

Water droplet sprays may be defined inside any compartment. Currently, the code assumes that a maximum mass of steam is condensed upon the droplets corresponding to the energy represented by the subcooling of the water delivered by the spray system. This is expected to be a good approximation to the spray effectiveness for the sizes of droplets produced by Soviet spray nozzles³ as indicated by the Soviet literature and for the fall heights characteristic of the accident localization volumes and containments. Cooling of the atmosphere by convective heat transfer to the droplets as well as by convective heat transfer to structural walls is not currently calculated.

Suppression pools may also be defined inside any compartment. Normally, when the pool water is subcooled, steam entering and passing through the pool is assumed to be condensed. If the pool water is heated to the local saturation temperature, then that fraction of the steam passing through the pool is assumed condensed which maintains the pool in a saturated state. Optionally, the user may specify that only a fraction (for example, zero) of the steam entering a subcooled pool is condensed to examine the effects of less than ideal performance of the pressure suppression system. The expulsion of slugs of water from the downward facing suppression pool trays incorporated in the VVER-440 Model V213 immediately following the onset of steam blowdown is modeled by the code.

It should be noted that the building loading calculations do not interactively account for the effects of gross structural failure or enhanced leakage which would tend to mitigate the predicted pressures.

Pipe Break Accidents

VVER-440 Model V230

Pressure and temperature loadings were calculated for the double-ended guillotine rupture of a 50 centimeter inner diameter outlet pipe. The break is located between the reactor vessel and the loop hot leg isolation valve. Volumes and condensation surface areas assumed for the multicompartment representation of the accident localization system are presented in table 1 and flow areas connecting the various localization compartments in table 2. The steam generator compartment was modeled as two subcompartments divided by an imaginary plane passing through the spent fuel pool and the linear cable corridor. This representation reflects the relatively small flow area joining the half of the steam generator room in which the break is located with the other half in which all three loops remain intact. The area connecting the steam generator compartment and the pump/deck compartment corresponds to the gaps surrounding the primary coolant pump motors and the isolation valve motors where the pump and valve motors pass through circular openings in the pump room floor. Soviet figures of the pump and its support⁴ suggest that a bellows-type seal of thin sheet steel normally seals the gap between each pump or valve and the surrounding steel shielding flange. However, Soviet calculations of the Cuban plant concept⁵ indicate that the seal is ruptured by a small pressure difference across the bellows. Accordingly, the area in table 2 was assumed throughout the calculation. The area between the steam generator compartment and the lower portion of the reactor shaft beneath the vessel support corresponds to the cross-sectional area at the upper part of the interconnecting vertical passageway. The steam generator room is connected to the middle portion of the reactor shaft which extends from the vessel support up to the bellows S-seal by the annular clearance gaps surrounding the twelve primary coolant pipes as they pass through circular penetrations in the shaft concrete wall. No credit is taken here for thermal insulation surrounding the piping. In particular, it is

assumed that the insulation would be crushed by the pressure loadings. A small leakage flow area of 0.2 square meters was assumed from the lower portion of the reactor shaft to the middle portion. This area corresponds to a 1 centimeter residual gap between the vessel support ring welded to the vessel and the vessel support flange atop the annular water shield tank. The upper portion of the reactor shaft above the bellows s-seal is assumed to be joined to the pump compartment by six circular pipes each having a diameter of 20 centimeters. The pipes are assumed to be normally sealed by rupture discs that open if the magnitude of the pressure differential between the upper shaft and pump compartment exceeds 0.1 megapascal.

The accident localization system incorporates vent valves which open when the local pressure exceeds 0.18 megapascal absolute.^{6,7} Based on Soviet literature suggests that the functioning of one valve provides an adequate venting capability for a 10 centimeter diameter primary system break with a unidirectional flow.⁸⁻¹⁰ The total valve vent capability is sized for a 20 centimeter diameter break and unidirectional flow.^{7,8} The calculations assume that nine vent valves are located in the half of the steam generator compartment containing the break. Each valve has a diameter of 1.0 meters and is assumed a discharge coefficient of 0.9⁸. One valve is assumed to open and close at an overpressure of 0.08 megapascal (as designed). The remaining eight valves are assumed to open and close at an overpressure of 0.10 megapascal (as designed).

An initial pressure of one atmosphere (~ 0.1 megapascal) and temperature of 50 degrees Centigrade were assumed inside the accident localization volume.¹¹ Figures 1 and 2 show the pressures and temperatures predicted inside the half of the steam generator compartment in which the break is located, inside the pump compartment, inside the lower portion of the reactor shaft, and inside the upper part of the reactor shaft above the S-seal over the first sixty seconds after the break. The pressure in the lower part of the shaft is virtually identical to that in the steam generator compartment reflecting the small lower shaft volume. For reference, the total flowrate of two-phase water from both ends of the break obtained from the calculations in Analysis Appendix B is included in the figures. In addition, the dashed line in figure 1 corresponds to the Soviet design pressure for the VVER-440 model V230 of 0.2 megapascal absolute (0.1 megapascal overpressure).^{7,12,13}

When the steam generator compartment pressure rises above 0.18 megapascal absolute the first vent valve begins to open one second after the break. The pressure reaches the design pressure, at which the remaining eight vent valves begin to open, at about two seconds following break inception. At this time, the rupture discs installed in the six pipes leading to the upper portion of the reactor shaft above the S-seal break, allowing steam to enter the upper shaft region. By eight seconds, the pressure inside the upper shaft region is calculated to attain the design value. Venting of steam and air through the flow area of the nine vent valves is effective in preventing the pressures inside the accident localization volume from exceeding the design pressure. Venting of steam and possibly radionuclide to the atmosphere continues for an interval of about fifty seconds.

After this time, the steam formation rate decreases near the end of the primary coolant system blowdown causing the pressures inside the accident localization volume to decrease and allowing the vent valves to close. Following closure of all of the valves, a portion of the air initially filling the accident localization volume has been discharged to the atmosphere. Operation of water droplet sprays to condense steam and reduce the temperature to 50 degrees Centigrade can decrease the pressure to a minimum value calculated to equal 0.073 megapascal absolute, corresponding to the pressure exerted by the air remaining inside the accident localization compartments. Thus, subatmospheric pressures may be attained inside the accident localization volume following the primary system blowdown venting phase. Subsequently, additional air is expected to leak back into the localization volume. Accident localization system leakage rates are relatively high.

The calculation was repeated assuming the operation of five vent valves instead of nine to examine the dependency of the peak pressure attained upon the venting capability. Four of the vent valves that open at an overpressure of 0.1

megapascal were assumed to remain closed. The results are shown in figures 3 and 4. The pressures inside the accident localization volume rise above the design value. A peak pressure of 0.27 megapascal absolute is calculated in all of the accident localization compartments at eleven seconds after the break. Functioning of all of the vent valves is thus calculated to limit the maximum pressure to 0.2 megapascal absolute or less. If the valves fail to open, then higher pressures are predicted.

To investigate the accident localization system pressurization existed in the complete absence of venting. It was assumed that none of the vent valves operate so no venting capability is realized. These calculations ignored structural failure of the accident localization compartment walls and floors. This case provides a calculation of the time dependent pressures which would be achieved in a closed volume system in the absence of venting, its structural failure figures 5 and 6. The pressure is calculated to rise to over 0.9 megapascal during the first minute after the break.

Pressure and temperature loadings were also predicted for the case of a primary system rupture having an equivalent diameter of 20 centimeters and unidirectional flow. This is the Soviet design basis for the capacity of the vent valves.^{7,8} The effects of steam condensation due to the water sprays were not modeled in these calculations for the VVER-440 model V230. The conditions of fluid immediately upstream of the break were not obtained from the Analysis Appendix B code calculations. The break conditions were estimated by modeling the primary system as a single volume inside which the primary coolant has a uniform pressure, temperature, and void fraction. The flowrate of the two-phase mixture through the rupture was calculated using a homogeneous equilibrium model for two-phase flow (which is usually limited by choking). This approach does not account for the hydraulic resistance effects of the primary piping, steam generator tubes, fuel channels, and so on, and is therefore expected to underestimate the overall timescale during which blowdown occurs. However, the single volume/homogeneous equilibrium approach provides a good prediction of the maximum pressure loading associated with the blowdown process. The primary system water was assumed to be initially saturated at a temperature of 284.5 degrees Centigrade equal to the mean of the VVER-440 inlet and outlet temperatures of 268 and 301 degrees Centigrade respectively. The primary system volume was taken equal to 221 cubic meters, consistent with the Analysis Appendix B large break calculations. Of the total primary inventory, 18 cubic meters were assumed to initially be steam, corresponding to the vapor space inside the pressurizer. The break was assumed to be located in the same half of the steam generator compartment as the primary pipe rupture in the large break case discussed above. Figures 7 and 8 show the pressure and temperature loadings calculated for the single-ended 20 centimeter break case. Fifteen seconds after the break, the pressures inside the accident localization compartments are calculated to rise to a value of 0.18 megapascal absolute, at which the first vent valve opens. Venting of steam and air through this single vent valve are calculated to limit the peak pressure to 0.184 megapascal absolute. The remaining eight valves are not calculated to open. This prediction is in contrast to the information presented in reference 8 which indicates that for a 20 centimeter diameter break with unidirectional flow, all of the valves open.

A nominal valve discharge coefficient equal to 0.90 has been assumed. An additional calculation was performed using a lower valve discharge coefficient of 0.62, to examine the effects of the valve discharge characteristics upon the pressure loadings. This represents a 31 percent decrease in the effective valve flow area. Figures 9 and 10 show the results for this case in which the pressure continues to rise to the design value of 0.20 megapascal at which the remaining eight valves open. Venting through all nine valves prevents further increase of the pressures inside the accident localization volume. At the design pressure, the rupture discs installed in the six pipes leading to the upper shaft region above the S-seal were assumed to open.

VVER-440 Model V213

Pressure and temperature loadings were calculated following the double-ended guillotine rupture of a 50 centimeter diameter primary outlet (hot leg) pipe at a location between the reactor vessel and the hot leg isolation valve. Peak loadings were also calculated for the rupture of a primary inlet (cold leg) pipe between the reactor vessel and cold leg

isolation valve. Volumes, condensation surface areas, and flow areas assumed in the multicompartment representation of the accident localization system are given in tables 3 and 4. The steam generator compartment was divided into two subcompartments similar to the VVER-440 model V230 analysis. The same flow area between the steam generator and pump rooms was assumed as in the VVER-440 model V230 calculations. In the VVER-440 model V213, a passageway does not directly connect the steam generator compartment with the lower portion of the reactor shaft. Access to the lower shaft is provided by a hatchway or airlock leading to the unsealed area beneath the steam generator compartment. Some leakage into the lower part of the shaft will take place through the gaps remaining between the vessel support ring and support flange upon which the vessel rests. Serpentine concrete is in a metal container in a recess in the reactor shaft concrete wall. The serpentine container is separated from the surrounding shaft concrete by a gap which allows for thermal expansion. The interconnecting flow area (table 4) models both a gap between the vessel and the support flange as well as leakage around the serpentine box. The volume of the middle part of the reactor shaft between the vessel support and the S-seal was lumped together with that of the steam generator compartment. The tunnel connecting the steam generator room with the bubbler-condenser tower is assumed to be two adjacent tunnels separated by a vertical wall partition. Each of the two tunnels was represented by a separate subcompartment in the calculation. The bubbler-condenser trays inside the bubbler-condenser tower are assumed to hold a total volume of borated water equal to 1250 cubic meters¹⁵ at a temperature of 50 degrees Centigrade. This is somewhat less than the volume of 1400 cubic meters assumed by the Soviets. A major determinant of the peak pressure attained in the VVER-440 model V213 accident localization system is the effective flow area of the check valve connecting the plenum region above each level of bubbler-condenser trays with the adjacent air trap. The effective flow area of the check valves was obtained from comparison with Soviet and Polish calculations^{16,17} of the VVER-440 model V213 concept carried out in the mid-nineteen seventies. Assuming a valve discharge coefficient of 0.9 (Soviet literature⁸), the resulting check valve diameter was determined to be 62 centimeters. This value is assumed in the current analysis. The resulting effective diameter of 59 centimeters is somewhat greater than the value of 50 centimeters assumed in Soviet calculations. The atmosphere inside the localization system was assumed to be of air at an initial pressure of one atmosphere and an initial temperature of 50 degrees Centigrade.

Figure 11 provides the pressures calculated for the large break case, the pressures inside the half of the steam generator compartment in which the break occurs, inside the vertical shaft convecting steam to the bubbler-condenser trays in the bubbler-condenser tower, and inside the air traps, during the first minute following inception of the pipe rupture. The total flowrate of two-phase mixture exiting from both sides of the break is also included for reference as well as the VVER-440 model V213 design pressure of 0.25 megapascal absolute (0.15 megapascal over-pressure).^{9,10,18-20} Temperatures are presented in figure 12. During the first minute, the local pressure reaches a maximum of 0.20 megapascal absolute, well below the design value. The relatively small flow area through the check valves into the air traps causes the pressure inside the air traps to initially lag behind that in the steam generator compartment and the bubbler-condenser tower vertical shaft. The air trap pressure is calculated to come to equilibrium with that in the remainder of the accident localization system in sixteen seconds.

One minute after the break occurs, a cumulative mass of 212 tonnes of fluid has been discharged through the break. At this time, 36.3 tonnes has been vaporized and introduced into the localization volume as steam. Of this steam mass, 26% (9.4 tonnes) has been condensed in the bubbler-condenser trays inside the bubbler-condenser tower. A steam mass of 5.50 tonnes has undergone condensation upon structure. The remaining steam mass of 21.4 tonnes is present as vapor inside the accident localization compartments. Thus, for the present large break loss-of-coolant accident, only a portion (about one-fourth) of the steam mass formed is predicted to be condensed in the bubbler-condenser trays the bubbler-condenser tower.

Sixty seconds after the pipe break,^{16,17} the sprinklers inside the steam generator compartment are assumed to begin spraying water into the steam generator compartment. The total spray flow rate was about 400 kilograms per second. This was based on Soviet literature discussing the effectiveness of the spray system in VVER-1000s.³ The total flow

rate of the three spray systems in VVER-440 model V-213S about 250 kilograms per second. This means that the calculations overpredict the effectiveness of the spray system on the order of 60%. The water droplets are assumed to have an initial temperature of 50 degrees Centigrade. Condensation of steam upon the injected falling water droplets together with condensation upon structure decreases the pressure inside the localization compartments. The calculations did not model the effects of passive spray phenomena associated with the expulsion of water from the bubbler-condenser trays backward through the steam channels leading to the trays. In Soviet calculations, the passive spray phenomena contribute an additional incremental reduction in the pressure, beginning approximately 30 seconds after a large-break loss-of-coolant accident. The reduction in pressure and temperature is shown in figures 13 and 14. The pressure is predicted to fall below one atmosphere at 8 minutes after the onset of the break. In contrast to the steam generator compartment pressure, the pressure inside the air traps remains constant as air is prevented from escaping by the check valves.

The sprays are assumed to be automatically switched off to prevent further decrease of the pressure when the local pressure in the steam generator compartment falls below 0.085 megapascal absolute. Subsequent steam formation causes the pressure to rise again. The sprays are automatically switched on again when the pressure equals to 0.095 megapascal absolute. The sprays are cycled on and off to maintain subatmospheric conditions inside the accident localization compartments.

If the sprinklers fail to inject water, then the pressure will be reduced by the effects of condensation upon structure and equipment alone. This is illustrated in figures 15 and 16 which show the pressures and temperatures calculated assuming that the sprays are inoperative. The pressure has decreased to 0.16 megapascal absolute which is still significantly above one atmosphere fourteen minutes after the break.

The spray system is designed to cycle on and off to maintain the pressure inside the localization system within the desired range. If the sprays come on but fail to cycle off, then continued condensation will reduce the pressure below the 0.085 megapascal shutoff setpoint. Figures 17 and 18 present the pressures and temperatures calculated when it was assumed that malfunctions prevent the sprays from being switched off. As shown in figure 17, the pressure is calculated to decrease to no less than 0.074 megapascal. This is the pressure exerted by the air which was not displaced into the air traps and has remained inside the accident localization compartments. During the initial steam blowdown phase, a portion of the air mass is calculated to be "dead-ended" into the pump compartment, reactor shaft, and the four ventilation compartments connected to the steam generator room. As the droplet sprays condense steam, then the air is uniformly distributed inside the accident localization compartments.

The operation of the sprays is further illustrated by figures 19 through 22 which show the results calculated for a primary outlet pipe break in which the water injected into the primary system by the emergency core cooling system is assumed (in the Analysis Appendix B calculations) to have a higher temperature of 111 degrees Centigrade. For this case, the sprays decrease the pressure to subatmospheric at 13 minutes after the break. Subsequent cycling of the sprays effects are shown in figure 21. The slope change at 12 minutes corresponds to the termination of the Analysis Appendix B calculation. Beyond this time, the steam formation rate is calculated from the time dependent core decay heat discussed in Background Appendix F. This analysis assumes that the reverse heat transfer from the secondary system to the primary system becomes relatively small compared with the decay heat by twelve minutes after the pipe rupture occurs.

During the spray cycling phase, subcooled water is being injected into the reactor vessel by the emergency core cooling system, low pressure pumps. As the water flows through the core, part of the decay heat is expended in removing the subcooling and the remainder goes into steam generation. The steam formed is condensed by the water droplet sprays. Condensed vapor plus liquid water which has flowed out of the break is collected by the sump system and nominally cooled by heat exchangers before being injected back into the vessel by the low pressure pumps or into the spray

headers by the spray pumps. The present analysis shows that for the assumed flowrate, the assumed operation of the sprays is effective in removing the core decay heat and maintaining subatmospheric conditions.

If the sprays fail to come on, then condensation of steam upon structure is predicted to be unable to establish subatmospheric conditions within forty minutes of the break for the assumed high emergency water temperature (figure 23). In particular, the pressure is reduced to 0.16 megapascal absolute over a forty minute interval. If the sprays fail to cycle off when the pressure falls below 0.085 megapascal, then the pressure is calculated to subsequently decrease to no less than 0.7 megapascal absolute (figure 25).

In figures 21, 23, and 25, the pressure inside the air traps is calculated to remain at the peak value attained during the blowdown phase. The calculations do not model leakage from the building associated with penetrations. In reality, air will gradually leak out of the air traps and air will leak into the accident localization compartments. The pressure inside the localization compartments is expected to increase back to atmospheric in about fifty hours. At this time the leakage rate from the accident localized compartment will be characteristic of the leak rate of the compartments at the pressure.

The short term loading of the localization volume was also predicted after a following the double-ended break of a primary inlet pipe. The pressures and temperatures during the first twenty seconds after a break are shown in figures 27 and 28. Relative to the hot leg break case (figure 11), the pressure after a cold leg rupture rises more rapidly reflecting the larger flowrate corresponding to emptying of the water below the core. The pressure in figure 27 exhibits a mild local peak at about six seconds, which was not obtained for the hot leg break. However, by 20 seconds after the break similar pressures are calculated for both the hot leg and cold leg break cases.

For the hot leg break case, several additional calculations were performed to examine specific aspects of and uncertainties in the VVER-440 model V213 accident localization system. First, the effects of the check valve size upon the peak pressure attained during the blowdown phase were investigated. The check valve diameter was decreased by 33% from a nominal diameter of 62 centimeters to 41 centimeters. This corresponds to a reduction in the total check valve area of 56% from 3.62 square meters to 1.61 square meters. The range of check valve sizes analyzed includes the check valve diameter of 50 centimeters use in calculations by the Soviets. For the smaller assumed check valve size (figures 29 and 30), the pressure inside the localization compartments exhibits a peak at about 13 seconds after the onset of the break. In addition, the pressure inside the air traps lags behind that in steam generator compartment by a longer interval. Specifically, the air trap pressure does not come to equilibrium with that in the other localization compartments until about 25 seconds after break inception compared with 16 seconds for the nominal check valve size.

Next, the effects of the bubbler-condenser trays failing to condense steam were examined. It was assumed that no steam condensed as it passed through the water pools in the trays. The results are shown in figures 31 and 32. The pressure was predicted not to exceed the design value even in the complete absence of any condensation in the bubbler-condenser tower trays. The pressure reaches a maximum of 0.24 megapascal absolute relative to the 0.25 megapascal design pressure. The out peak pressure level in the absence of condensation results from the large total volume of the VVER-440 model V213 accident localization system, which was increased relative to that of the VVER-440 model V230. As noted earlier, when the bubbler-condenser tower functions in an ideal as-designed fashion, only one-fourth of the steam generated during the first minute after the pipe rupture is calculated to be condensed in the trays. Because the peak pressure lies nearly 0.05 megapascal below the design pressure in that case, failure to condense any steam at all in the water pools does not result in pressures exceeding the design limit. In addition to pressure suppression, the trays also serve another function which is to separate steam from the air which flows into the air traps. Failure of the bubbler-condenser tower to condense steam results in steam entering the air traps together with air. This reduces the amount of air which can be removed from the accident localization compartments making the subsequent creation and maintenance of subatmospheric conditions by water spray condensation more difficult.

Next, the effects of uncertainties in the accident localization system volume upon the peak pressure were investigated. In particular, the four ventilation related compartments connected to the steam generator compartment were assumed to be inaccessible to the flow of steam and air. This reduces the total localization system sealed volume assumed in the calculation from the nominal value of 52,500 cubic meters to 43,700 cubic meters. This volume is close to the value of 45,000 cubic meters assumed in Soviet calculations. Figures 33 and 34 present the results calculated for the first minute following pipe rupture. For the lower total localization volume, the pressure is predicted to achieve a maximum of 0.215 megapascal absolute. In this case, a total steam mass of 35.8 tonnes is formed during the first minute of which 27.6% (9.87 tonnes) is condensed in the bubbler-condenser trays and 5.22 tonnes is condensed upon structure.

To examine the effects of uncertainties in the spray system flowrate, the flowrate was reduced by nearly a factor of two from 403 to 210 kilograms per second. This value is quoted in reference 11 as the spray system flowrate for the VVER-1000s that were then under development. It is also somewhat less than the flow rate of 250 kilograms per second reported by the Soviets to be delivered by the three spray systems in the VVER-400 model V213. The spray dominated decrease in the localization system pressure is shown in figures 35 and 36. For the smaller water delivery rate, the pressure now decreased to subatmospheric sixteen minutes after the break.

VVER-1000 Model V320

Pressure and temperature loadings inside a VVER-1000 model V320 containment following the double-ended guillotine rupture of an 85 centimeter inner diameter primary coolant pipe were calculated. The cases of pipe breaks in both the outlet leg as well as the inlet leg were both considered. The containment was represented in terms of the three compartments indicated in tables 5 and 6. The first compartment, referred to as the "containment upper region," consists of the containment dome interior, the interior rooms surrounding the steam generators, pressurizer, accumulators, and primary piping, the volume surrounding the primary coolant pumps, and the upper portion of the reactor shaft above the vessel support. The second compartment is the largely annular room immediately above the containment basemat in which containment ventilation systems are located. The inner boundary of this room is the outer wall of the lower part of the reactor shaft. The lower portion of the reactor shaft constitutes the third compartment. With the exception of the basemat, the internal floors inside the containment do not extend to the containment shell inner surface but are separated from the shell by a gap of variable thickness around the circumference of the shell. This gap is reflected in the flow area connecting the containment upper region with the lower annular compartment. The area joining the lower portion of the reactor shaft with the lower annular compartment corresponds to the estimated cross-sectional area of the passageway penetrating the shaft wall. A small leakage flow in the region of reactor vessel support connects the lower part of the shaft with the upper region of the containment.

Figures 37 and 38 present the predicted pressures and temperatures inside the three compartments for the first minute after the break for the case of a hot leg primary coolant pipe rupture. Virtually identical pressures are calculated in all compartments. A maximum pressure of 0.32 megapascal absolute (0.22 megapascal overpressure) is predicted which is 0.19 megapascal below the assumed containment design pressure of 0.51 megapascal absolute (0.41 megapascal overpressure).^{6,9,10,13,21} Temperatures are calculated to rise by no more than 140 Centigrade degrees which is less than the design temperature rise of 150 Centigrade degrees.

A total cumulative water mass of 367 tonnes has exited one minute after the break. The cumulative steam mass formed equals 67.4 tonnes of which 3.23 tonnes have condensed upon structure and equipment. The VVER-1000 model V320 has three circular spray headers in the containment dome. The sprays begin injection of water droplets one minute after break inception. Based upon Soviet analyses of the spray system effectiveness in the VVER-1000, the spray flowrate was assumed to be 403 kilograms per second.³ This value is equal to two-thirds of the total flowrate of approximately 600 kilograms per second reported by the Soviets to be delivered by the three spray systems. The calculation corresponds to the case in which one of the spray systems is assumed not to function. The water delivered

by the sprays was assumed to have an initial temperature of 50 degrees Centigrade relative to a value of 60 degrees Centigrade assumed in Soviet calculations. Pressures and temperatures predicted for a time interval of twenty minutes after the onset of the break are shown in figures 39 and 40. The sprays are calculated to effectively condense all of the excess steam inside the containment by 18 minutes. At this time, the pressure has fallen to a value of 0.12 megapascal absolute. The pressure does not immediately return to one atmosphere because the containment temperature is higher than the initial value. Subsequently, all steam formed is promptly condensed by continued operation of the sprays.

To further delineate the effects of condensation upon structure, the calculation was repeated for the blowdown phase with the condensation rate deliberately set equal to zero. In the absence of structure-induced condensation, a slightly higher peak pressure of 0.33 megapascal would be obtained (figures 41 and 42) which is only 0.01 megapascal above that obtained for the nominal condensation rates.

Pressure and temperature loadings for the double-ended rupture of a cold leg primary coolant pipe are presented in figures 43 and 44. Relative to the hot leg break, much higher break flowrates are obtained during the first 8 seconds following pipe rupture. Accordingly, higher containment pressures are predicted during the first 30 seconds after the break inception. However, following that time, similar maximum pressures of 0.32 and 0.315 megapascal absolute are respectively obtained for both outlet and inlet leg break locations. Following activation of the sprinkler system, the pressure would decrease in a similar manner to that calculated for the outlet pipe failure.

Steam Generator Manifold Failure

Pressure and temperature loadings were calculated for a failure of a steam generator internal manifold. Calculations were carried out only for the VVER-440 model V213. The failure is assumed to occur in the hot leg manifold which delivers primary coolant to the steam generator from the reactor vessel. The break was assumed to be a long tear along the height of the manifold. In particular, the break area is assumed to exceed that for flow from the primary outlet pipe and from the steam generator tube bank.

Of principal interest was whether or not the failure of the manifold could directly result in failure of the steam generator shell and allow secondary coolant to escape into the accident localization volume. The pressures achieved in the secondary side volume of the steam generator following manifold failure are dependent upon a number of complex and interacting phenomena including the conditions of primary coolant exiting the break, condensation of steam generated from flashing of primary coolant upon secondary water, venting of steam through the steam collector and main steamline, and expulsion of liquid water into the steam collector and main steamline.

A major determinant of the pressure the steam generator shell is the effectiveness with which steam formed by flashing of primary fluid can be vented through the steamline. Estimates of the steam generator shell pressurization immediately following manifold failure were carried out for the cases of pure steam flow through the steamline and complete blocking of the steamline by slugs of liquid water discharged into the collector or steamline. The multicompartment blowdown analysis code was used to predict the time dependent pressure inside the steam generator. The vapor space inside the shell was taken equal to 22 cubic meters. The most effective pressure relief corresponds to the venting of pure steam through the 45 centimeter inner diameter steamline. The conditions of primary fluid immediately upstream of the manifold break used were those calculated (Analysis Appendix B) for the double-ended guillotine break of a primary outlet pipe. Assuming that the steam formed by flashing of primary fluid is vented directly through the steamline and ignoring the condensation of steam upon secondary water, figure 45 shows the pressure inside the steam generator shell for the first 10 seconds following manifold failure. The pressure is calculated to rise by 1.6 megapascal from the nominal secondary side pressure of 4.7 megapascals to a peak value of 6.3 megapascals by 0.2 seconds after manifold failure. This value is an estimated lower bound to the steam generator shell pressurization which can be attained.

Significantly higher pressures may be obtained inside the shell if water is discharged into the steam collector or main steamline. Slugs of liquid water will restrict the flow of steam resulting in greater pressures. Specifically, the pressure would be expected to rise to essentially the primary system pressure upstream of the ruptured manifold. This is illustrated by figure 46 which shows an estimate of the shell pressurization during the first second immediately following manifold failure, assuming no venting of steam from the shell. In this case, the pressure is estimated to rise by 4.8 megapascals to 9.5 megapascals which is the primary system pressure just upstream of the manifold break. This pressure rise is achieved over an interval of approximately 0.6 second. If shell failure does not occur, then the secondary region of the steam generator would be expected to fill with primary fluid and primary fluid will be discharged through the steamline. In the absence of shell failure, the cross-sectional area for depressurization of the primary system will eventually be the steamline. Since the flow area of the steamline is less than half that for the double-ended rupture of a primary coolant pipe, the depressurization history of the primary system will ultimately differ significantly from that for a double-ended guillotine pipe rupture.

The structural response calculations discussed in Analysis Appendix E indicate that shell failure will not occur if the pressures do not exceed the nominal primary system operating pressure of 12.5 megapascals. However, the structural analysis did not account for the effects of shell penetrations such as the manifolds, manway covers, feedwater line, steam collector, or inspection viewport as well as the weldments of the elliptical heads to the cylindrical shell. If the vessel were to fail, the predicted indicated failure mode is a tear along the length of the steam generator cylindrical shell away from the elliptical head in the region of lower shell thickness.

Pressures and temperatures inside the accident localization volume were calculated assuming a shell rupture which results in the formation of a very large breach area. Immediately following the formation of a large breach in the steam generator shell, the secondary side region inside the shell will rapidly depressurize to the ambient pressure inside the steam generator localization compartment. Due to the large size of the opening, the depressurization process would not be expected to be significantly limited by compressible flow effects such as choked flow at the shell rupture. Following depressurization, the secondary and primary water inside the shell will be in a highly superheated state. Homogeneous nucleation of vapor bubbles would be expected to take place within the bulk of the liquid inside the shell. With the formation of vapor bubbles throughout the liquid, the water inside the steam generator shell will froth up into a bubbly two-phase mixture. At a sufficiently high vapor fraction (for example, 0.9), the vapor will disengage from the liquid leaving behind droplets and ligaments of liquid. Most of the steam generated will be the result of flash vaporization from the surfaces of the remaining liquid droplets. The accident localization volume loading calculation was initiated at the time when shell rupture has occurred, the vapor has just disengaged from the liquid, and significant vapor production from flashing is commencing. Steam generation from the flashing of water droplets was calculated for the 43 cubic meters of secondary water assumed to be initially in the steam generator. The water was assumed to be at a temperature of 260 degrees Centigrade corresponding to saturation conditions at the secondary side operating pressure of 4.7 megapascals. The subsequent rate of steam formation from the superheated liquid will depend upon the size of the droplets generated. The droplet size was assumed to be given by the correlation of De Jarlais, Ishii, and Linehan²² for the maximum stable droplet size following droplet creation and entrainment in the presence of a gas stream. This correlation predicts a maximum water droplet diameter of 1.2 millimeters for the conditions of the VVER-440 model V213 system. Following shell rupture and depressurization, the flowrate and specific internal enthalpy of the primary coolant escaping through the failed manifold are expected to resemble those conditions following the double-ended break of a primary outlet pipe. Consequently, the total steam formation rate inside the localization volume was assumed to correspond to the flashing of secondary water from the failed steam generator plus a simultaneous double-ended outlet pipe rupture. Figures 47 and 48 show the pressures and temperatures inside the half of the steam generator compartment containing the failed shell, in the vertical shaft inside the bubbler-condenser tower, and inside the air traps over a time interval of 10 seconds, assuming that the bubbler-condenser trays inside the bubbler-condenser tower function in the ideal as-designed manner. Also included in figures 47 and 48 are the steam formation rate from flashing of secondary water initially residing inside the breached steam generator and the discharge

rate of primary coolant from the failed manifold. Flashing-induced vaporization of the steam generator secondary inventory is predicted to take place over a short timescale less than one second in duration. During this time, steam generation results in a pressure spike inside the portion of the steam generator compartment containing the failure. The pressure attains a peak value of 0.21 megapascal absolute. More significantly, the pressure is predicted not to exceed the accident localization volume design pressure.

Of the 33.6 tonnes of secondary water initially present in the steam generator, 8.3 tonnes flashes to vapor. At 10 seconds after shell breaching, 18.2 tonnes of steam have also been formed from the discharge of primary fluid through the manifold. Of the total steam mass produced (26.5 tonnes), 7.0 tonnes has been condensed inside the bubbler-condenser trays and 1.06 tonne condensed upon structure and equipment.

As secondary water from the hypothetically failed steam generator flashes to steam, the velocities of steam and air passing through the bubbler-condenser trays exceed those obtained over the longer timescale characteristic of a primary coolant pipe break. To examine the effects of less than ideal performance of the bubbler-condenser trays, the calculation of loadings of the accident localization volume was repeated assuming that no condensation takes place in the trays. The results for this case are shown in figures 49 and 50. It is observed that virtually the same pressure spike occurs in the steam generator compartment whether or not condensation takes place in the bubbler-condenser trays. Thus, even in the absence of condensation in the water pools, the pressure loadings immediately following a steam generator shell failure are predicted not to exceed the design pressure.

The analysis did not calculate the longer term incremental pressurization of the localization volume resulting from the flashing of secondary coolant elsewhere on the secondary side distinct from the failed steam generator. Further analyses would be necessary to determine if the combined loading from the blowdown of both the complete primary and secondary systems into the localization volume would exceed the Soviet design pressure or cause structural failure. The effects of the primary system pressurizing the secondary system were not addressed in this analysis.

Reactor Vessel Failure

VVER-440 Model V230

Pressure loadings inside the VVER-440 model V230 accident localization system were calculated for the hypothetical case of the failure of the reactor pressure vessel in the core region. The vessel is hypothesized to be breached by the propagation of a crack in an embrittled core region weldment which joins together adjacent cylindrical forgings. The crack is hypothesized to propagate around the complete circumference of the vessel splitting the vessel into two distinct pieces.

A blowdown of the primary system into the lower portion of the reactor shaft will take place immediately after the break. The blowdown rate is assumed to initially be limited by the effective flow area of the 15 centimeter gap between the reactor vessel outer wall and the inner wall of the annular wall shield tank. The gap flow area is equal to 1.88 square meters and would be equivalent in size to a 1.55 meter diameter circular hole. Although thermal insulation is present in the gap volume, the insulation is assumed to be crushed by the high pressures achieved following vessel failure. Accordingly, no credit was taken for occlusion of the gap volume by the insulation. It should be noted that the gap area is less than that of the downcomer region inside the reactor vessel.

The conditions of fluid immediately upstream of the breach were obtained by modeling the entire primary system as a single volume, inside which the primary coolant has a uniform pressure, temperature, and void fraction. The flowrate of two-phase mixture through the breach was calculated using a homogeneous equilibrium model for two-phase flow which may be limited by choking. This approach does not account for the hydraulic resistance effects of the fuel

channels, primary piping, steam generator tubes, and so on, and is therefore expected to underestimate the overall timescale during which blowdown occurs. However the single volume/homogeneous equilibrium approach provides a good prediction of the maximum pressure loading associated with the blowdown process. The primary system water was assumed to be initially saturated at a temperature of 268 degrees Centigrade, equal to the VVER-440 inlet temperature. This assumption is appropriate for the calculation of pressure loads immediately after the hypothesized beltline failure in which fluid from the reactor vessel below-core region is first discharged through the break. The total primary system volume was taken equal to 221 cubic meters, of which 18 cubic meters is initially steam (corresponding to the vapor space inside the pressurizer).

The multicompartment representation of the accident localization volume employed three compartments corresponding to the steam generator compartment, the pump room, and the lower portion of the reactor shaft beneath the vessel support. The middle portion of the reactor shaft between the vessel support and the S-seal was lumped together with the steam generator room. Volumes, condensation surface areas, and interconnecting flow areas for the localization system are presented in tables 1 and 2. The lower shaft region is assumed to be connected to the steam generator compartment by the vertical passageway having a relatively small flow area of only 1.43 square meters. A small leakage flow area of 0.2 square meters from the lower part of the shaft to the middle shaft region was modeled in the calculation. A reference vent valve capability consisting of four vent valves each having a diameter of 62 centimeters and characterized by a discharge coefficient of 0.9 was assumed. Pressures inside the lower portion of the shaft were predicted on the basis of the flow of pure steam through the mostly vertical passageway joining the lower shaft region with the steam generator compartment. The effects of the simultaneous dispersal of water (which would tend to restrict the steam flow thereby resulting in greater shaft pressures than those predicted here) were not accounted for in the calculations.

The pressures predicted during the first twenty seconds following vessel failure are shown in figure 51. Pressures as high as 2.5 megapascals absolute were calculated the lower portion of the reactor shaft within one second after vessel failure. The pressure significantly exceeds the VVER-440 model V230 design pressure of 0.2 megapascal^{7,12,13} absolute indicated by the dashed line in figure 51. The high pressures in the lower portion of the shaft are a consequence of the small lower shaft region volume and the small relief area through the vertical passageway. The complete primary system blowdown is predicted to take only about eight seconds. Choking of the steam flow from the lower shaft region to the steam generator compartment is indicated at the exit of the vertical passageway over most of this interval.

The steam flow venting through the vertical passageway into the steam generator compartment causes the steam generator compartment pressure to exceed the design value one second after vessel failure. The steam generator compartment pressure also significantly exceeds the design pressure, attaining a peak value of 0.55 megapascal absolute. The maximum pressure inside the steam generator room considers venting of steam through the vent valves. After the primary system blowdown is completed, the pressure inside the localization volume decreases as steam continues to be discharged through the vent valves.

Pressures as high as 2.5 megapascals might result in gross failure of the reactor shaft concrete walls allowing steam to be vented into the soil surrounding the shaft which protrudes into the ground. The creation of additional venting pathways from the lower part of the shaft would tend to reduce the peak pressures in the lower shaft region.

The reactor vessel is not held down upon the vessel support but rests upon the support flange atop the annular water shield tank. If structural failure of the shaft wall does not open up additional vent paths which mitigate the pressures inside the lower shaft region, then the pressure in the lower shaft may be sufficient to physically lift the vessel off of its support. The differential forces across the reactor pressure vessel shown in figure 52 are calculated for the lower shaft pressure shown in figure 51. The force is the difference between the pressures in the lower shaft region and the ambient reactor bay pressure above the reactor vessel upper head times the cross-sectional area of the lower part of

the vessel. The lower dashed line in figure 52 (5.7 meganewtons) corresponds to the weight of a VVER-440 model V230 reactor assembly, 573 tonnes.²³ The upward force is predicted to exceed that necessary to levitate the vessel within a fraction of a second following vessel failure.

If the vessel is lifted off of its support, then the upward motion of the vessel will be initially restrained by the twelve primary coolant pipes welded to the vessel nozzles. In VVER-440s, model V230s, each primary pipe passes through a circular penetration embedded in the shaft cylindrical concrete wall. As the vessel moves upward, the motion of each pipe is limited by the shaft concrete. Continued application of the upward force to the vessel will cause the piping to bend between the vessel and the shaft wall. Because the weld metal joining the pipes to the nozzles is not as strong as the piping material itself, bending and potential failure of the pipes is expected to be most pronounced near the nozzle attachments. If the bending stresses in the piping locally exceed the ultimate tensile strength, then the pipes will fail in a tearing mode.

[The net force upon the vessel which will cause the pipes to be torn off the vessel can be estimated from the bending stress formula,²⁴

$$s = \frac{Fxy}{I_x} \quad (1)$$

where

- s = local stress in pipe,
- F = bending force acting upon pipe,
- x = moment arm,
- y = distance from neutral x axis,
- I_x = moment of inertia-about x axis,

where

$$I_x = \frac{\pi}{64} (D_o^4 - D_i^4) \text{ for a circular pipe,} \quad (2)$$

- D_o = pipe outer diameter,
- D_i = pipe inner diameter.

For a cylindrical pipe, the maximum stress occurs at $y = D_o/2$. The moment arm length, x , is assumed to be 2 meters (representative of the distance between the vessel outer wall and the shaft inner wall in the pipe penetration region). Assuming an ultimate strength of 510 megapascals for VVER-440 piping stainless steel,²⁵ the force required to fail one pipe is 1.7 meganewton. The total net force required to tear all twelve pipes off of the vessel is twelve times this or 21 meganewtons. Consequently, the pressure differential force across the vessel must be greater than this value plus the gravitational weight force equal to a total of 26 meganewtons. The upper dashed line in figure 52 corresponds to this value. The upward force upon the vessel is predicted to attain a maximum value of 24 meganewtons, which is slightly less than that required to fail of the piping. The peak upward force is similar to the estimate of the piping failure threshold. The tearing off of the twelve primary coolant pipes is a distinct possibility, the consequences of which are examined further.

The carbon steel bellows provides some restraint of the vessel. The bending stress formula can be used to provide an estimate of the net force upon the vessel which will cause the bellows to fail in a tearing mode. For the bellows, the moment of inertia may be approximated by

$$I_x = \frac{1}{12} b h^3 \quad (3)$$

where b is the circumference of the bellows about the vessel and h is the bellows thickness. Assuming a bellows thickness of 0.4 centimeter, a moment arm of 1 meter, and an ultimate tensile strength for carbon steel of 432 megapascals,²⁶ the force required to fail the bellows is calculated to be 0.014 meganewton. This is a relatively insignificant force compared with that needed to tear off the primary coolant pipes.

If the twelve pipes are torn off of the vessel and the vessel continues to move upward, then the upper block containing the thirty-seven control rod drives will impact upon the 38 tonne²⁷ protective dome over the reactor shaft. The dome is held down upon the shaft by approximately sixty bolts. The impacting upper block structure could puncture the 2.5 centimeter thick dome steel and tear an opening through the dome. A conservative criterion for failure of the dome may be obtained by estimating the energy required to fail all of the bolts securing the dome to the shaft. In particular, the minimum energy required is given by:

$$\epsilon_t = n_b \frac{\pi}{4} D_b^2 L_b \int_0^{\epsilon_{ult}} \sigma(\epsilon) d\epsilon \quad (4)$$

where

- n_b = number of bolts,
- D_b = bolt diameter,
- L_b = bolt length,
- $\sigma(\epsilon)$ = stress corresponding to the strain, ϵ ,
- ϵ = strain,
- ϵ_t = total strain,
- ϵ_{ult} = strain corresponding to the ultimate tensile stress.

The bolts are assumed to have a diameter of 5 centimeters and a length of twenty centimeters. The bolting material is assumed to have a yield stress of 720 megapascals at a strain of 0.0015 and an ultimate tensile stress of 810 megapascals at a total strain of 0.15. For these values, the integral is estimated to have a value of 120 megapascals.

A conservative estimate of the energy to fail sixty bolts is 2.9 megajoules. This energy corresponds to the kinetic energy of a mass equal to that of the reactor assembly (573 tonnes) moving with a velocity of 3.2 meters per second. Included in figure 52 is a calculation of the upward displacement of a body having a weight equal to that of the reactor assembly in response to the pressure differential force in the absence of any restraint by the piping. The unmitigated forces resulting from steam formation following vessel breaching would accelerate an unrestrained vessel to a velocity of 10 meters per second over a distance of 10 meters (figure 52).

Following failure of the S-seal and the dome holddown, a blowdown of steam from the accident localization volume directly into the reactor bay would take place.

VVER-1000 Model V320

Pressure loadings inside a VVER-1000 model V320 containment were calculated following the hypothetical failure of the reactor pressure vessel in the core region. The vessel was assumed to fail by the growth of a crack in an embrittled wall which propagates around the complete vessel circumference splitting the vessel into two distinct segments.

Immediately following the failure, a blowdown of the primary system would take place into the lower portion of the reactor shaft. The blowdown rate will initially be restricted by the effective flow area of the downcomer gap between the core barrel outer surface and the inside of the vessel wall. The gap flow area in the core region is 2.7 square meters and the effective break area is therefore equal to twice this value or 5.4 square meters. The breach area is equivalent to that from a 2.6 meter diameter circular hole with unidirectional flow. It should be noted that the effective break area limited by the downcomer size is smaller than that corresponding to the gap between the reactor vessel and the surrounding shaft wall. Also, the downcomer gap area is less than the total flow area of the eight primary coolant pipes.

The blowdown of the primary system was calculated using the single volume/homogeneous equilibrium approach, assuming that the primary system fluid is initially saturated at a temperature of 305.5 degrees Centigrade equal to the mean of the VVER-1000 inlet and outlet temperatures of 289 and 322 degrees Centigrade respectively. This assumption is appropriate for predicting the peak containment pressure loading immediately following the blowdown process. The total primary system volume was assumed to be 310 cubic meters of which 24 cubic meters is initially steam representing the vapor space inside the pressurizer.

The multicompartment representation of the containment employed three compartments corresponding to the upper portion of the containment, the mostly annular lower compartment surrounding the lower part of the reactor shaft, and the lower portion of the reactor shaft itself beneath the vessel support ledge. Volumes, condensation surface areas, and interconnecting flow areas are shown in tables 5 and 6. The lower shaft region is connected to the surrounding annular compartment by a horizontal passageway with a flow area of approximately 5.4 square meters. During normal operation, the exit of the passageway is covered with a concrete slab. However, following the shaft pressurization, the slab would be expected to be displaced from its normal position before the shaft concrete walls fail thereby providing the principal vent pathway from the lower shaft. A small leakage flow area of 0.2 square meters from the lower shaft to the upper containment region was assumed in the calculations. The pressure inside the lower shaft region was predicted on the basis of the flow of pure steam through the horizontal passageway leading to the lower annular compartment. The effects of the simultaneous dispersal of water which would tend to restrict the steam flow and thereby result in greater shaft pressures than those predicted here were not accounted for in the calculations.

The pressures predicted during the first 2.5 seconds after vessel failure are shown in figure 53. Due to the large effective break area, the primary system is calculated to depressurize in this short timescale. The pressure inside the lower shaft region is calculated to rise to a peak value of 4.0 megapascals absolute 0.3 seconds after failure. Although the lower shaft pressures significantly exceed the VVER-1000 model V320 design pressure of 0.51 megapascal absolute,^{6,9,10,13,21}

the pressures inside the lower annular compartment and the containment upper region (which are directly applied to the containment cylindrical shell and elliptical dome) remain below the design value. Thus, failure of the cylindrical shell and dome due to overpressure alone is not predicted immediately after the rupture of the pressure vessel. The containment design pressure has been defined to bound the equilibrium pressure attained following the depressurization of the entire primary system inventory inside the closed volume of the containment shell. The lower boundary of the reactor shaft region is the containment basemat. Consequently, the high pressures achieved in the lower part of the shaft are applied directly to the containment bottom boundary interior to the shaft wall. If gross structural failure of the basemat occurs, then steam and fission products would be released directly into the unsealed part of the reactor building under the containment.

As discussed in Analysis Appendix E, overpressures of the order on 2.8 megapascals or greater will cause gross structural failure of the lower shaft concrete walls. The additional relief path area associated with shaft failure will tend to mitigate the pressures such that the peak value in the lower shaft will be less than the 4 megapascal value indicated by the present calculation. The basemat in the shaft region is not predicted to undergo gross failure (Analysis Appendix E) for overpressures below 4 megapascals.

The VVER-1000 model V320 reactor pressure vessel is not held down but rests on an annular support structure atop a ledge in the reactor shaft wall. Following vessel failure, the upward differential pressure force across the reactor vessel would rapidly exceed that required to lift the vessel off of its support as shown in figure 54. The lower dashed line in figure 54 at 7.2 Meganewtons represents the gravitational force corresponding to the weight of the reactor assembly which is taken equal to 730 tonnes.²³ Although the upward forces across the vessel are several times those needed to levitate the vessel off of its support, the restraint provided by the primary piping is too great to permit the vessel to be lifted. The net force upon the vessel required to tear off one primary coolant pipe was estimated from the bending stress formula, equation 1, together with the moment of inertia for a circular pipe given by equation 2. The moment arm was assumed to be 0.67 meters (corresponding to the distance between the vessel outer wall and the shaft inner surface). Assuming an ultimate strength of 490 megapascals²⁵ for VVER-1000 piping carbon steel base metal, the force needed to fail one pipe is predicted to be 32 Meganewtons. In addition to the primary coolant piping, four accumulator injection lines are attached to the reactor vessel in the vicinity of the primary coolant nozzles. The force required to tear off an individual accumulator pipe is 2.6 Meganewtons. For eight primary pipes plus four accumulator lines, the total net force to tear off all the vessel piping is therefore 266 Meganewtons. This, plus the weight, is more than a factor of six times the peak upward force calculated to act upon the reactor vessel (figure 54). The total upward force of 273 Meganewtons required to tear off all of the piping would correspond to a pressure of the order of 25 megapascals inside the lower shaft region. Consequently, the failure of the vessel is predicted not to result in failure of the attached piping and lofting of the vessel upward out of the lower shaft region.

The force required to tear off a single primary pipe from the VVER-1000 model V320 vessel is nearly twenty times as great as that predicted for the VVER-440 model V230. The greater effective strength of the VVER-1000 model V320 piping principally reflects the larger pipe diameter and wall thickness combined with the dependency of the moment of inertia in equation 2 upon the difference of the fourth powers of the pipe outer and inner diameters. In addition, the force is inversely proportional to the moment arm length which in the VVER-1000 model V320 system is one-third that of the VVER-440 model V230.

Control Rod Ejection-Induced Failure in Vessel Upper Head Region

This initiator is the ejection of a single control assembly that results in the rupture of the control assembly drive housing located atop the reactor vessel upper head permitting the escape of primary coolant. Following formation of a coolant boundary rupture in the upper head region, flashing of primary coolant to steam will pressurize the upper portion of the reactor shaft above the bellows S-seal and under to the steel dome. Pressure loadings were calculated using the

same multicompartment representation of the VVER-400 model V230 accident localization volume assumed in the analysis of primary coolant pipe ruptures. Volumes, condensation surface areas, and intercompartment flow areas are given in tables 1 and 2. The effects of steam condensation due to the operation of water droplet sprays was not modeled.

Figure 55 shows the penetration through the upper head associated with each control assembly. The flowrate of coolant through the break is limited by the area of the 9 centimeter diameter shroud tube surrounding the control assembly driveline. The conditions of primary fluid immediately upstream of the break were calculated by modeling the entire primary system as a single volume inside which the primary coolant has a uniform pressure, temperature, and void fraction. The flowrate of two-phase fluid through the break was calculated using the homogeneous equilibrium model for two-phase flow which may be limited by choking. Because this approach does not account for the hydraulic resistance effects of the fuel channels, primary piping, steam generators, and so on, it is expected to somewhat underestimate the overall timescale over which the blowdown process takes place. However, the single volume/homogeneous equilibrium approach does provide a good estimate of the maximum pressure loading resulting from the blowdown. For the head release calculations, the primary system water was assumed to be initially saturated at a temperature of 284.5 degrees Centigrade equal to the mean of the VVER-440 inlet and outlet temperatures of 268 and 301 degrees Centigrade respectively. The primary system volume was assumed to be 221 cubic meters. Of the total primary inventory, 18 cubic meters was assumed to initially be steam (corresponding to the vapor space inside the pressurizer).

Figures 56 and 57 show the pressures and temperatures calculated in the accident localization volume 3 minutes after the break. The pressure inside the upper portion of the reactor shaft (above the S-seal), rises to the design pressure of 0.20 megapascal absolute within 5 seconds after a break. At this pressure level, the rupture discs installed inside the six pipes leading from the upper shaft region open, allowing primary coolant to flow into the pump and steam generator compartments. Discharge of steam from the upper part of the shaft causes the upper shaft region pressure to temporarily decrease to 0.17 megapascal absolute at 23 seconds. In the absence of activation of water droplet sprays, the pressure inside the steam generator compartment increases to a value of 0.18 megapascal at which the first vent valve opens, 110 seconds after the break. Venting of steam and air to the atmosphere through this single valve prevents the steam generator compartment pressure from rising above 0.18 megapascal absolute. While the vent valve remains open, the corresponding pressure inside the upper shaft region is calculated to achieve and maintain a value of about 0.21 megapascal absolute. This pressure slightly exceeds the design basis value.

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**Table 1 Volumes and Condensation Surface Areas Assumed in Analyses of
VVER-440 Model V230 Accident Localization System**

	Volume, cubic meters	Condensation surface area, square meters
Steam Generator Compartment	8,580	5,696
Pump/Deck Compartment	2,608	1,700
Lower Portion of Reactor Shaft	123	0
Middle Portion of Reactor Shaft	132	0
Upper Portion of Reactor Shaft Above S-seal	427	222
Total	11,870	7,618

**Table 2 Flow Areas Assumed in Analysis of VVER-440
Model V230 Accident Localization System**

	Area, square meters
Steam Generator Compartment to Pump Compartment	4.15
Steam Generator Compartment to Lower Portion of Reactor Shaft	1.43
Steam Generator Compartment to Middle Portion of Reactor Shaft	7.69
Lower Portion of Reactor Shaft to Middle Portion of Reactor Shaft	0.2
Steam Generator Compartment to Atmosphere	6.36 ¹ , 0.707 ²
Half of Steam Generator Compartment Containing Break to Other Half of Steam Generator Compartment	38.0
Upper Portion of Reactor Shaft Above S-seal to Pump Compartment	0.188 ³

¹Area when the steam generator compartment pressure exceeds 0.20 megapascal absolute.

²Area when the steam generator compartment pressure exceeds 0.18 megapascal absolute.
For lower pressures, the area is set equal to zero.

³Area after the pressure differential exceeds 0.10 megapascal. Prior to this, the area
is set equal to zero.

**Table 3 Volumes and Condensation Surface Areas Assumed in Analyses of
VVER-440 Model V213 Accident Localization System**

	Volume, cubic meters	Condensation surface area, square meters
Steam Generator Compartment¹	11,620	7,779
Pump/Deck Compartment	2,487	1,678
Lower Portion of Reactor Shaft	316	0
Tunnels Leading to Bubbler-Condenser Tower	1,116	820
Vertical Shaft Inside Bubbler-Condenser Tower	7,862	11,150
Plenum Regions Above Bubbler-Condenser Trays	4,107	0
Air Traps	16,220	7,196
Ventilation Compartment Besides Half of Steam Generator Compartment with Break	1,058	725
Ventilation Compartment Besides Half of Steam Generator Compartment without Break	1,745	1,136
Ventilation Compartment Besides Tunnel and Half of Steam Generator Compartment with Break	2,966	1,326
Ventilation Compartment Besides Tunnel and Half of Steam Generator Compartment without Break	2,966	1,326
Total	52,460	33,140

¹Includes middle portion of reactor shaft

**Table 4 Flow Areas Assumed in Analyses of VVER-440 Model V213
Accident Localization System**

	Area, square meters
Steam Generator Compartment to Pump Compartment	4.15
Steam Generator Compartment to Lower Portion of Reactor Shaft	1.70
Half of Steam Generator Compartment Containing Break to Other Half of Steam Generator Compartment	34.1
Steam Generator Compartment to Tunnels Leading to Bubbler-Condenser Tower	67.6
Tunnels to Vertical Shaft Inside Bubbler-Condenser Tower	67.6
Entrance Channels to Bubbler-Condenser Trays	499
Bubbler-Condenser Trays to Plenum Regions Above Trays	499
Plenum Regions to Air Traps	3.621
Steam Generator Compartment to Each of the Four Ventilation Compartments	37.2

¹Area when plenum region pressure exceeds air trap pressure. For plenum region pressures below the air trap pressure, the area is set equal to zero.

**Table 5 Volumes and Condensation Surface Areas Assumed in Analysis of
VVER-1000 Model V320 Containment**

	Volume, cubic meters	Condensation surface area, square meters
Containment Upper Region	61,850	19,050
Lower Annular Compartment Surrounding Lower Portion of Reactor Shaft	6,082	3,796
Lower Portion of Reactor Shaft	155	0
Total	68,090	22,850

**Table 6 Flow Areas Assumed in Analysis of VVER-1000
Model V320 Containment**

	Area, square meters
Containment Upper Region to Lower Annular Compartment	200
Lower Annular Compartment to Lower Portion of Reactor Shaft	5.40
Containment Upper Region to Lower Portion of Reactor Shaft	0.2

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**Figure 1 Pressure Loading of VVER-440 Model V230
Accident Localization Volume Following Primary Coolant Outlet
Pipe Break for Case of Venting Through Nine Vent Valves**

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**Figure 2 Temperature Loading of VVER-440 Model V230
Accident Localization Volume Following Primary Coolant
Outlet Pipe Break for Case of Venting Through Nine Vent Valves**

**Figure 3 Pressure Loading of VVER-440 Model V230
Accident Localization Volume Following Primary Coolant
Outlet Pipe Break for Case of Venting Through Five Vent Valves**

**Figure 4 Temperature Loading of VVER-440 Model V230
Accident Localization Volume Following Primary Coolant
Outlet Pipe Break for Case of Venting Through Five Vent Valves**

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**Figure 5 Pressure Loading of VVER-440 Model V230
Accident Localization Volume for Primary Coolant
Outlet Pipe Break Without Venting Through Vent Valves**

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**Figure 6 Temperature Loading of VVER-440 Model V230
Accident Localization Volume for Primary Coolant
Outlet Pipe Break Without Venting Through Vent Valves**

**Figure 7 Pressure Loading of VVER-440 Model V230 Accident
Localization Volume Following 20 Centimeter Diameter
Break With Unidirectional Flow for Case
Vent Valve Discharge Coefficient Equal to 0.90**

**Figure 8 Temperature Loading of VVER-440 Model V230
Accident Localization Volume Following 20 Centimeter Diameter
Break With Unidirectional Flow for Case of
Vent Valve Discharge Coefficient Equal to 0.90**

Figure 9 Pressure Loading of VVER-440 Model V230
Accident Localization Volume Following 20 Centimeter Diameter Break
With Unidirectional Flow for Case of Vent Valve Discharge Coefficient Equal to 0.62

**Figure 10 Temperature Loading of VVER-440 Model V230
Accident Localization Volume Following 20 Centimeter Diameter Break
With Unidirectional Flow for Case of Vent Valve Discharge Coefficient Equal to 0.62**

**Figure 11 Pressure Loading of VVER-440 Model V213
Accident Localization Volume Following Primary Coolant Outlet Pipe Break**

**Figure 12 Temperature Loading of VVER-440 Model V213
Accident Localization Volume Following Primary Coolant Outlet Pipe Break**

**Figure 13 Pressure Loading of VVER-440 Model V213 Accident
Localization Volume Following Primary Coolant Outlet Pipe Break**

**Figure 14 Temperature Loading of VVER-440 Model V213
Accident Localization Volume Following Primary Coolant Outlet Pipe Break**

**Figure 15 Pressure Loading of VVER-440 Model V213 Accident
Localization Volume Following Primary Coolant Outlet Pipe Break
for Case of Water Droplet Sprays Inoperative**

**Figure 16 Temperature Loading of VVER-440 Model V213 Accident
Localization Volume Following Primary Coolant Outlet
Pipe Break for Case of Water Droplet Sprays Inoperative**

**Figure 17 Pressure Loading of VVER-440 Model V213 Accident
Localization Volume Following Primary Coolant Outlet Pipe Break
for Case of Water Droplet Sprays Failing to Cycle Off**

**Figure 18 Temperature Loading of VVER-440 Model V213 Accident
Localization Volume Following Primary Coolant Outlet Pipe Break for Case
of Water Droplet Sprays Failing to Cycle Off**

**Figure 19 Pressure Loading of VVER-440 Model V213 Accident
Localization Volume Following Primary Coolant Outlet
Pipe Break for Case of High Emergency Core Cooling Water Temperature**

**Figure 20 Temperature Loading of VVER-440 Model V213 Accident
Localization Volume Following Primary Coolant Outlet Pipe Break
for Case of High Emergency Core Cooling Water Temperature**

Figure 21 Pressure Loading of VVER-440 Model V213 Accident Localization Volume Following Primary Coolant Outlet Pipe Break for Case of High Emergency Core Cooling Water Temperature

**Figure 22 Temperature Loading of VVER-440 Model V213 Accident
Localization Volume Following Primary Coolant Outlet Pipe Break
for Case of High Emergency Core Cooling Water Temperature**

Figure 23 Pressure Loading of VVER-440 Model V213 Accident Localization Volume Following Primary Coolant Outlet Pipe Break for Case of High Emergency Core Cooling Water Temperature and Water Droplet Sprays Inoperative

Figure 24 Temperature Loading of VVER-440 Model V213 Accident Localization Volume Following Primary Coolant Outlet Pipe Break for Case of High Emergency Core Cooling Water Temperature and Water Droplet Sprays Inoperative

Figure 25 Pressure Loading of VVER-440 Model V213 Accident Localization Volume Following Primary Coolant Outlet Pipe Break for Case of High Emergency Core Cooling Water Temperature and Failure of Water Droplet Sprays to Cycle Off

**Figure 26 Temperature Loading of VVER-440 Model V213 Accident
Localization Volume Following Primary Coolant Outlet Pipe
Break for Case of High Emergency Core Cooling Water Temperature
and Failure of Water Droplet Sprays to Cycle Off**

Figure 27 Pressure Loading of VVER-440 Model V213 Accident Localization Volume Following Primary Coolant Inlet Pipe Break

**Figure 28 Temperature Loading of VVER-440 Model V213 Accident
Localization Volume Following Primary Coolant Inlet Pipe Break**

**Figure 29 Pressure Loading of VVER-440 Model V213 Accident
Localization Volume Following Primary Coolant Outlet
Pipe Break for Case of Reduced Check Valve Diameter**

**Figure 30 Temperature Loading of VVER-440 Model V213 Accident
Localization Volume Following Primary Coolant Outlet Pipe Break
for Case of Reduced Check Valve Diameter**

**Figure 31 Pressure Loading of VVER-440 Model V213 Accident
Localization Volume Following Primary Coolant Outlet Pipe Break
for Case of No Condensation in Bubbler-Condenser Trays**

**Figure 32 Temperature Loading of VVER-440 Model V213 Accident
Localization Volume Following Primary Coolant Outlet Pipe
Break for Case of No Condensation in Bubbler-Condenser Trays**

**Figure 33 Pressure Loading of VVER-440 Model V213 Accident
Localization Volume Following Primary Coolant Outlet Pipe Break
for Case of Reduced Accident Localization Volume**

**Figure 34 Temperature Loading of VVER-440 Model V213 Accident
Localization Volume Following Primary Coolant Outlet Pipe Break
for Case of Reduced Accident Localization Volume**

**Figure 35 Pressure Loading of VVER-440 Model V213 Accident
Localization Volume Following Primary Coolant Outlet Pipe Break
for Case of Reduced Water Droplet Spray Flowrate Equal
to 210 kilograms per second**

**Figure 36 Temperature Loading of VVER-440 Model V213 Accident
Localization Volume Following Primary Coolant Outlet Pipe Break
for Case of Reduced Water Droplet Spray Flowrate Equal
to 210 kilograms per second**

Figure 37 Pressure Loading of VVER-1000 Model V320 Containment Following Primary Coolant Outlet Pipe Break

**Figure 38 Temperature Loading of VVER-1000 Model V320 Containment
Following Primary Coolant Outlet Pipe Break**

**Figure 39 Pressure Loading of VVER-1000 Model V320 Containment
Following Primary Coolant Outlet Pipe Break**

**Figure 40 Temperature Loading of VVER-1000 Model V320 Containment
Following Primary Coolant Outlet Pipe Break**

**Figure 41 Pressure Loading of VVER-1000 Model V320 Containment
Following Primary Coolant Outlet Pipe Break for Case of
No Condensation Upon Structure and Equipment**

**Figure 43 Pressure Loading of VVER-1000 Model V320
Containment Following Primary Coolant Inlet Pipe Break**

Figure 42 Temperature Loading of VVER-1000 Model V320 Containment Following Primary Coolant Outlet Pipe Break for Case of No Condensation Upon Structure and Equipment

Figure 44 Temperature Loading of VVER-1000 Model V320 Containment Following Primary Coolant Inlet Pipe Break

Figure 45 Pressure Loading of VVER-440 Model V213 Steam Generator Shell Following Rupture of Internal Hot Leg Manifold for Case of Venting of Pure Steam Through Main Steamline

Figure 46 Pressure Loading of VVER-440 Model V213 Steam Generator Shell Following Rupture of Internal Hot Leg Manifold for Case of Discharge of Pure Liquid Through Main Steamline

**Figure 47 Pressure Loading of VVER-440 Model V213 Accident
Localization Volume Following Rupture of Internal Hot Leg Manifold
and Breaching of Steam Generator Shell**

Figure 48 Temperature Loading of VVER-440 Model V213 Accident Localization Volume Following Rupture of Internal Hot Leg Manifold and Breaching of Steam Generator Shell

**Figure 49 Pressure Loading of VVER-440 Model V213 Accident
Localization Volume Following Rupture of Internal Hot Leg Manifold
and Breaching of Steam Generator Shell for Case of
No Condensation in Bubbler-Condenser Trays**

**Figure 50 Temperature Loading of VVER-440 Model V213 Accident
Localization Volume Following Rupture of Internal Hot Leg Manifold
and Breaching of Steam Generator Shell for Case of
No Condensation in Bubbler-Condenser Trays**

**Figure 51 Pressure Loading of VVER-440 Model V230 Accident
Localization Volume Following Failure of Reactor Pressure Vessel**

Figure 52 Upward Force Loading Upon VVER-440 Model V230 Reactor Pressure Vessel Following Failure of Reactor Pressure Vessel

Figure 53 Pressure Loading of VVER-1000 Model V320 Containment Following Failure of Reactor Pressure Vessel

Figure 54 Upward Force Loading Upon VVER-1000 Model V320 Reactor Pressure Vessel Following Failure of Reactor Pressure Vessel

**Figure 55 Detail of Control Rod Driveline Assembly Penetration
Through Upper Head of VVER-440 Reactor Vessel
(all dimensions are in millimeters)**

**Figure 56 Pressure Loading of VVER-440 Model V230 Accident
Localization Volume Following
Control Drive Assembly Housing Failure**

**Figure 57 Pressure Loading of VVER-440 Model V230 Accident
Localization Volume Following Control Drive Assembly Housing Failure**

ANALYSIS APPENDIX D
FUEL CORE/TEMPERATURE ANALYSES

Table of Contents

1.0 Introduction	1
2.0 VVER-440 Model V230 Large Break Loss of Coolant Accidents	2
2.1 Thermal Hydraulic Model	2
2.2 Results	2
3.0 Loss of Flow Events	3
3.1 Thermal Hydraulic Modeling	3
3.2 VVER-440 Model V230 Natural Circulation Results	3
3.3 VVER-1000 Model V320 Single Pump Seizure Results	4
4.0 Surge Line Breaks	4
4.1 Thermal Hydraulic Modeling	4
4.2 Results	5

List of Figures

1. RETRAN-02/MOD4 Nodalization
2. VVER-440 Model V-230 Cold Leg CORE Temperatures
3. VVER-1000 Model V320 Nodalization
4. Flow Rate in the Hot Fuel Channel of a VVER-440 Model V230 with All Pumps Tripped
5. Flow Rate in the Average Fuel Channel of a VVER-440 Model V230 with All Pumps Tripped
6. Flow Rate in the Average Control Assembly Channel of a VVER-440 Model V230 with All Pumps Tripped
7. Void Fraction in the Hot Fuel Channel of a VVER-440 Model V230 with All Pumps Tripped
8. Void Fraction in the Average Fuel Channel of a VVER-440 Model V230 with All Pumps Tripped
9. Void Fraction on the Average Control Assembly Channel of a VVER-440 Model V230 with All Pumps Tripped
10. Fuel Clad Temperature in the Hot Fuel Channel of a VVER-440 Model V230 with All Pumps Tripped
11. Fuel Clad Temperatures in the Average Fuel Channel of a VVER-440 Model V230 with All Pumps Tripped
12. Fuel Clad Temperatures in the Fuel Follower of the Average Control Assembly Channel of a VVER-440 Model V230 With All Pumps Tripped
13. Flow Rate in the Loop Representing Five Loops of a VVER-440 Model V230 with All Pumps Tripped
14. Flow Rate in the Single Loop of a VVER-440 Model V230 with All Pumps Tripped
15. Void Fraction in the Upper Plenum Region of a VVER-440 Model V230 with All Pumps Tripped
16. Void Fraction in the Hot Leg of a Single Loop of a VVER-440 Model V230 with All Pumps Tripped
17. Flow Rate in Loop with Stopped Pump in a VVER-1000 Model V320
18. Flow Rate in the Hot Fuel Channel of a VVER-1000 Model V320 with One Stopped Pump
19. Flow Rate in the Average Fuel Channel of a VVER-1000 Model V320 with One Stopped Pump
20. Flow Rate in the Average Control Channel of a VVER-1000 Model V320 with One Stopped Pump
21. Fuel Clad Temperature in the Hot Fuel Channel of a VVER-1000 Model V320 with One Stopped Pump
22. Fuel Clad Temperature in the Average Fuel Channel of a VVER-1000 Model V320 with One Stopped Pump
23. Fuel Clad Temperature in the Average Control Channel of a VVER-1000 Model V320 with One Stopped Pump
24. Flow Rate in Active Loop of a VVER-1000 Model V320 with One Stopped Pump
25. RELAP MOD2/ISPRA Nodalization of VVER-440 Heat Transport System
26. Break Flow Rate Following Surge Line Break in a VVER-440 Model V230
27. Primary System Pressure During Surge Line Break in VVER-440 Model V230
28. Core Inlet and Outlet Temperature During Surge Line Break in a VVER-440 Model V230
29. Fuel Pin Clad Axial Temperature Distribution During Surge Line Break in a VVER-440 Model V230
30. Primary System Pressure During a Surge Line Break in a VVER-440 Model V213
31. Emergency Coolant Flow From Accumulators During A Surge Line Break in a VVER-440 Model V213
32. Break Flow Rate Following a Surge Line Break in a VVER-440 Model V213
33. Core Inlet and Outlet Temperatures During a Surge Line Break in a VVER-440 Model V213
34. Fuel Pin Clad Axial Temperature Distribution During a Surge Line Break in a VVER-440 Model V213

ANALYSIS APPENDIX D

FUEL CORE/TEMPERATURE ANALYSES

1.0 Introduction

The loss of coolant accident analyses (Analysis Appendix B) and reactivity analyses (Analysis Appendix H) were done using simplified thermal hydraulic system models. Specifically, the core was modeled using a single average fuel pin and the primary system was modeled using a fairly coarse nodalization. The results of the analyses, coupled with the information obtained from the review of VVER accident analyses (Background Appendix B) identified several transients that needed a more detailed nodalization either in the core, primary system, secondary system, or a combination of these. This appendix summarizes the modeling and results of analyses for these transients.

The first transient considered was a large break loss of coolant accident in a VVER-440 model V230. The single pin model used in Analysis Appendix B showed that large scale voiding of the core occurred very early in the transient. A three channel core model, consisting of a hot fuel channel, average fuel channel, and average control channel, was constructed for the RETRAN-02/MOD4 code. RETRAN-02 was considered to be applicable for this case since the VVER-440 model V230 does not have an emergency core cooling system. There is an emergency core cooling system injecting 100 cubic meters (433 gallons per minute) into the cold legs of three of the six loops. This arrangement was not expected to lead to counter current flow situations.

The review of the accident analyses, Background Appendix B, showed concern, by the Soviets and other operators of VVERs, with loss of flow events in VVERs, especially those involving loss of pumping power in the VVER-440 model V230 and VVER-1000 model V320. These events are of concern for two reasons. First, in VVERs, the unit may be run with one or more pumps shut down. The reactor power is reduced to a specified level consistent with a power to flow ratio for the number of operating pumps in all VVERs unless the majority of the pumps lose power. Therefore, there are several different initial conditions for loss of flow events. Second, the VVER-400 model V230 uses low inertia, canned rotor pumps which have an extremely short coastdown period. The VVER-400 model V230 has special initial power generators that are designed to extend the coastdown period. Loss of pumping power is of concern, especially in the cases where the unit is already running on a reduced number of pumps. The RETRAN-02 code was used for these transients because of its sophisticated control system model.

Small break loss of coolant accidents have been recognized as being potentially serious for several years. These accidents are of particular concern for the VVER-440 since this reactor has two loop seals in each primary coolant system thermal hydraulic loop (before and after the steam generator). These loop seals not only depress the coolant level in the core during loss of coolant accidents but also have a significant effect on the heat transfer through the steam generators. Because of the potentially serious consequences of the loop seal effects, the Loviisa units have had several modifications made to the system to reduce or eliminate the loop seals. These modifications included adding drain connections between the hot and cold legs on the sides of the loop seals closest to the reactor.

A surge line break in a VVER-440 model V230 was selected as a small break loss of coolant accident. The surge line for the pressurizer is the largest diameter connection to the primary coolant system. The transient would proceed relatively quickly compared to more conventional small break loss of coolant accidents, e.g. stuck open pressurizer relief valves. However, in the VVER-440 model V230 there are two surge lines connected to the primary system. Moreover, the pipe stubs connected to the pressurizer are built up from weld metal at the site. These two factors make such a break more likely in a VVER-440 than in comparable United States plants. The RELAP5/MOD2 code was used for these transients because two phase phenomena were expected to be of importance.

Small break loss of coolant accidents with break sizes smaller than 20 centimeters and steam line break accidents are two classes of transients with potentially serious consequences. These transients require detailed steam generator modeling with two phase effects accounted for on both the primary and secondary sides. The horizontal steam generators used in the VVER design have features not found in United States designs that require advanced thermal hydraulic models to simulate properly. These transients should be simulated using the advanced EPRI developed code RETRAN-03.

2.0 VVER-440 Model V230 Large Break Loss of Coolant Accidents

A detailed core model analysis of a double ended cold leg primary coolant pipe break in a VVER-440 model V230 was done because the single pin model used in Analysis Appendix B indicated that core damage since large scale voiding of the core occurred very early in the transient. The VVER-440 model V230 has a limited emergency makeup capability of 100 cubic meters per hour (433 gallons per minute). A simple energy balance calculation showed that this flow might be sufficient to cool the core via steam cooling if all the fluid were to reach the core region. However, this fluid is injected into the cold legs of the primary coolant system piping and therefore likely to bypass the core region. Because large injections of emergency core cooling water are not possible in the VVER-440 model V230, the RETRAN-02/MOD-4 code was used for this analysis.

2.1. Thermal Hydraulic Model

The RETRAN-02 MOD4 nodalization is shown in figure 1. A total of 35 volumes was used to represent the primary coolant system. The core region was divided into three channels: 1) a hot fuel channel, 2) an average fuel channel, and 3) an average control assembly channel. The fuel channels were divided into four axial nodes consisting of an unheated inlet nozzle region and three axial heated regions. A chopped cosine distribution was used to calculate the axial power distribution. The average control assembly channel consisted of one heated node representing the fuel follower, an unheated region with increased flow resistance representing the boron steel control assembly, and two unheated axial nodes of lower flow resistance representing the control assembly drive line. The control assembly fuel follower was located below the core region (that is in the fully inserted or scrammed position) at all times.

The primary heat transport system was modeled using two loops; one loop representing five lumped loops. The steam generators were modeled using one average tube. The pressurizer safety valves and spray line were explicitly modeled; the pressurizer heaters were not modeled. The break was simulated by opening and closing valves over 0.2 seconds in the single loop. The emergency makeup flow was modeled as entering the primary system at the pump suction side with two thirds of the total flow, 67 cubic meters per hour, (~ 300 gallons per minute) entering the intact (lumped) loop and one third 33 cubic meters per hour (~ 150 gallons per minute) entering the broken loop. The injection pressure and trip setting for the VVER-440 model V230 were not known; the appropriate values for the high pressure coolant injection system of the VVER-440 model V213 were used.

2.2 Results

The transient was started at full power and full flow. The double ended pipe break was simulated by opening and closing valves in the cold leg over 0.2 seconds. The primary coolant pumps were tripped at 0.05 seconds and reactor scram initiated at 1.0 seconds. The break flows, which are choked, are discussed in Analysis Appendix B. As was the case there, the core volumes attain 100% quality within 12 seconds of the break initiated. Clad surface temperatures, Figure 2, rise quickly during this time, attaining 700 degrees centigrade (1300 degrees Fahrenheit) in the same time frame. The temperatures remain fairly constant until 50 seconds, when total dryout occurs and the clad temperature starts to rise again, eventually reaching 900 degrees centigrade (1,800 degrees Fahrenheit). At 90 seconds into the transient, the simulation was terminated. It is estimated that the clad temperatures will reach 1200°C (2200 degrees Fahrenheit) at approximately 140 seconds.

Particular attention was paid to the fuel follower of the control assembly. This assembly is located beneath the core region in the scram position. Therefore, this assembly received some cooling water through the majority of the transient and did not undergo a significant heatup until 60 seconds into the transient.

The emergency feed system injected approximately 180 lbs per second of water once injection started. This is approximately twice the break flow at the time the transient was terminated. However, none of this water was entering the core region, which was in a downward flow regime.

3.0 Loss of Flow Events

Two loss of flow events were simulated: 1) a complete loss of power to all six pumps in a VVER-440 model V230 with reactor scram; and 2) a seizure of one pump in a VVER-1000 model V320 with no scram. The complete loss of power to all pumps in the VVER-440 model V230 is not considered a credible event by the Soviets due to the diversity of the power sources for the primary coolant pumps.

3.1 Thermal Hydraulic Modeling

The RETRAN-02 MOD4 model for the VVER-440 model V230 was identical to that used in the large break loss of coolant accidents discussed above (figure 1). The core was orificed so that the two fuel channels had large pressure drops (amounting to approximately half the pressure drop through the assemblies) in the inlet orifice regions. The control assembly channel, which is physically located within a circular guide tube, was modeled with a large flow resistance at the entrance to the guide tube representing a hydraulic brake which is placed there to slow the control assembly insertion. Additionally, the guide tube takes suction from a location below the core region. The fuel follower was modeled as being in the below core location at all times. The flow was modeled as discharging from the top of the guide tube through two sets of outlets located at different axial locations.

The VVER-1000 model V320 nodalization is shown in figure 3. In this model, one hot fuel channel, one average fuel channel, and one average control channel were represented in the core region. Unlike the VVER-440, the control assembly channels do not differ substantially from the fuel channels as reactivity control is maintained using borated steel pins which are inserted into locations within fixed fuel assemblies. In a normal fuel assembly, fixed burnable poison pins occupy the same locations as the control pins. The core channels were modeled with four axial locations, the lower three nodes being heated and the topmost node being unheated. A chopped cosine distribution was used to determine the axial power profile. A point kinetics model was used, with reactivity feedback effects accounted for on a local basis.

The VVER-1000 model V320 assemblies are open and unshrouded, unlike the VVER-440 assemblies which are located in hexagonal shrouds. Therefore, cross flow between the core channels is likely in the VVER-1000 model V320 is likely. Cross-flow effects were not accounted for in the core model used because of inadequate information on bundle performance.

3.2.1 VVER-440 Model V230 Natural Circulation Results

The primary coolant pumps of the VVER-440 model V230 have extremely low inertias which leads to extremely short coastdown times. To increase the time the pumps run after a loss of power event, four of the six pumps are connected to inertial power generators (which are connected to the turbine generator shafts). The other two pumps are connected to an offsite power source. Because of this diversity of power sources, the Soviets do not consider loss of power to all six pumps to be a creditable event.

The loss of power to all six pumps event was first simulated using the single average fuel pin model developed for the loss of coolant accidents described in Analysis Appendix B. This model indicated that in the long term, natural circulation would be established.

A detailed core model was developed to study this event in the short term. In modeling loss of flow events, detailed information on the pressure drops in the primary heat transport system is extremely important. The known pressure drops, however, were limited to the reactor pressure vessel nozzle to nozzle pressure drop and the steam generator primary side pressure drop, both were only known at full flow conditions. The remaining pressure drop information was assigned to various components based on the known geometry and total available pump head. The pressure loss coefficients calculated using this information may not be representative of the plant.

The coolant flow rate decreases rapidly in 3-5 seconds (figure 4-6). Significant voiding occurs in the core channels within 5 seconds (figure 1-9). This is accompanied by flow reversals in the core channels. The voiding does not last long enough to significantly increase the clad surface temperatures (figures 10-12). The flow rate remains positive in the simulated loops throughout the transient (figures 13-14). Voiding does occur in the upper plenum region (figure 15) and in the hot leg connected to the pressurizer (figure 16).

3.2.2 VVER-1000 Model V320 Single Pump Seizure Results

The transient was started at full power and full flow conditions. A single pump was tripped at 0.05 seconds and the speed set to zero using the RETRAN pump seizure option at 0.1 seconds. In this transient, scram, which would normally occur on a pressure differential across the pump signal, was inhibited. The transient was simulate for 60 seconds.

The primary coolant flow reverses with 2 seconds in the loop with the stopped pump (figure 17). This is accompanied by a decrease in the core flow rate to 70% of full flow conditions (figures 18-20). The decrease is uniform in all the channels. The decreased core flow rate leads to increases in the clad surface temperatures of approximately 15 degrees Fahrenheit (figure 21-23). The flow increases by approximately 10% in the active loops (figure 24).

The reactivity feedback mechanisms cause the power to decrease, eventually obtaining a new steady state value of 89% of full power. This increased power to full ratio of 1.23 is reflected in a decreasing minimum departure from nucleate boiling ratio, which drops from 2.2 at steady state to 2.0 at the termination of the transient.

4.0 Surge Line Breaks

Surge line breaks were simulated for the VVER-440 model V230 and model V213. The surge line is 20 centimeters (~ 8 inches) in diameter. Therefore, this transient represents an intermediate size break which proceeds somewhat more slowly than the double ended 50 centimeter (~ 20 inches) breaks. However, it is expected that the majority of energy is removed from the primary system through the break rather than through the steam generators. Therefore, a fairly simple steam generator model can be used.

4.1 Thermal Hydraulic Modeling

The RELAP5 MOD2-ISPRA code was used to simulate this event. This code is a version of RELAP5 MOD2 which was modified at the Joint Research Center in Ispra for running on IBM type machines. The nodalization used is shown in figure 25. As can be seen, particular attention was paid to the loop seals in the primary heat transport loops. The core was modeled using only one average fuel pin, with three axial nodes. The downcomer was split, with individual connections to the lumped intact loop and the single loop connected to the pressurizer. The accumulators were modeled explicitly for the VVER-440 model V213. There are no accumulations in the VVER-440 model V230. The reactor was assumed to be at full power and full flow conditions.

The steam generators were modeled using one average tube. In order to simplify the model on the secondary side, the feedwater was assumed to enter the steam generator at the bottom of the shell side rather than through a sparger located midway up the tube bundle. The steamline was modeled as a boundary condition. During the transient, the

feedwater was shut off and the steam generators allowed to bleed off steam to the steamline.

The pressurizer was modeled as having two surge lines for the VVER-440 model V230 and one surge line for the VVER-440 model V213. The two surge lines for the VVER-400 model V230 were connected to the same loop. It is not known if this is the case in actual plants. The break was actually modeled by the opening and closing of two sets of valves.

4.2 Results

The RELAP5 Mod 2 code was run for 100 seconds in both cases in order to eliminate any steady state-transient mismatches. This 100 seconds period has been eliminated from the plots shown in this section.

The results of the surge line break in a VVER-440 model V230 are shown in figures 26-29. The break flow (figure 26) is calculated using a critical flow model over the first 40 seconds of the transient. The system pressure (figure 27) decreases smoothly over the entire transient reflecting the lack of an emergency core cooling system in this model VVER. The core inlet and outlet temperature difference (figure 28) collapses quickly after the initiation of the break. The core temperature then decreases in response to the reactor depressurization. The core starts to dry out in the upper part at approximately 220 seconds into the transient as shown by the temperature in the uppermost axial node of the fuel pin (figure 29). By 320 seconds, the entire core region has dried out (as shown by the temperature increase in the lowest node in figure 28). Extrapolating the temperature curves in figure 4.5 indicates that the fuel pin cladding temperature will reach 1200 degrees Centigrade at approximately 550 seconds.

The VVER-440 model V213 results shown in figure 30-34 reflect strongly the effects of the emergency core cooling systems present in this reactor model. The depressurization (figure 30) follows the same trend as the model V230 until approximately 30 seconds into the transient when the accumulators first start to inject into the vessel upper plenum and downcomer region. (The accumulator flow is shown in figure 31). This first injection of accumulator water is sufficient to restore the system pressure (through vaporization of the injected accumulator water) to a level sufficient to shut off accumulator injection at approximately 50 seconds. The system pressure remains constant until approximately 125 seconds into the transient. At this point the accumulators start to inject again, however the reactor has cooled sufficiently that repressurization does not occur. The break flow (figure 32) increases at this time reflecting the fact that subcooled water is now exiting the break location.

The core inlet and outlet temperatures are show in figure 33. Unlike United State designs, the VVER-440 model V213 accumulators inject directly into the upper plenum and downcomer region of the vessel. This dual injection allows the vessel to refill from the top in a counter current flow regime during the course of this transient. This is reflected in the core outlet temperature which is lower than the core inlet temperature during the portion of the transient when the accumulators are injecting. The fuel pin axial temperatures (figure 34) also show this temperature inversion.

In this transient, the accumulators had emptied by 175 seconds. The transient calculation was stopped at this time.

Figure 1 RETRAN-02/MOD4 Nodalization

Figure 2 VVER-440 Model V-230 Cold Leg CORE Temperatures

Figure 2 VVER-1000 Model V320 Nodalization

**Figure 3 Flow Rate in the Hot Fuel Channel of a VVER-440 Model V230
with All Pumps Tripped**

Figure 4 Flow Rate in the Average Fuel Channel of a VVER-440 Model V230 with All Pumps Tripped

Figure 5 Flow Rate in the Average Control Assembly Channel of a VVER-440 Model V230 with All Pumps Tripped

**Figure 6 Void Fraction in the Hot Fuel Channel of a VVER-440 Model V230
with All Pumps Tripped**

**Figure 7 Void Fraction in the Average Fuel Channel of a VVER-440 Model V230
with All Pumps Tripped**

Figure 8 Void Fraction on the Average Control Assembly Channel of a VVER-440 Model V230 with All Pumps Tripped

Figure 9 Fuel Clad Temperature in the Hot Fuel Channel of a VVER-440 Model V230 with All Pumps Tripped

Figure 10 Fuel Clad Temperatures in the Average Fuel Channel of a VVER-440 Model V230 with All Pumps Tripped

Figure 11 Fuel Clad Temperatures in the Fuel Follower of the Average Control Assembly Channel of a VVER-440 Model V230 With All Pumps Tripped

Figure 12 Flow Rate in the Loop Representing Five Loops of a VVER-440 Model V230 with All Pumps Tripped

**Figure 13 Flow Rate in the Single Loop of a VVER-440 Model V230
with All Pumps Tripped**

Figure 14 Void Fraction in the Upper Plenum Region of a VVER-440 Model V230 with All Pumps Tripped

Figure 15 Void Fraction in the Hot Leg of a Single Loop of a VVER-440 Model V230 with All Pumps Tripped

Figure 16 Flow Rate in Loop with Stopped Pump in a VVER-1000 Model V320

Figure 17 Flow Rate in Loop with Stopped Pump in a VVER-1000 Model V320

Figure 18 Flow Rate in the Hot Fuel Channel of a VVER-1000 Model V320 with One Stopped Pump

Figure 19 Flow Rate in the Average Fuel Channel of a VVER-1000 Model V320 with One Stopped Pump

**Figure 20 Flow Rate in the Average Control Channel of a VVER-1000 Model V320
with One Stopped Pump**

Figure 21 Fuel Clad Temperature in the Hot Fuel Channel of a VVER-1000 Model V320 with One Stopped Pump

Figure 22 Fuel Clad Temperature in the Average Fuel Channel of a VVER-1000 Model V320 with One Stopped Pump

Figure 23 Fuel Clad Temperature in the Average Control Channel of a VVER-1000 Model V320 with One Stopped Pump

Figure 24 Flow Rate in Active Loop of a VVER-1000 Model V320 with One Stopped Pump

**Figure 25 RELAP MOD2/ISPRA Nodalization of VVER-440
Heat Transport System**

Figure 26 Primary System Pressure During Surge Line Break in a VVER-440 Model V230

Figure 27 Core Inlet and Outlet Temperature During Surge Line Break in a VVER-440 Model V230

Figure 28 Fuel Pin Clad Axial Temperature Distribution During Surge Line Break in a VVER-440 Model V230

Figure 29 Primary System Pressure During a Surge Line Break in a VVER-440 Model V213

Figure 30 Emergency Coolant Flow From Accumulators During A Surge Line Break in a VVER-440 Model V213

Figure 31 Break Flow Rate Following a Surge Line Break in a VVER-440 Model V213

Figure 32 Core Inlet and Outlet Temperatures During a Surge Line Break in a VVER-440 Model V213

Figure 33 Fuel Pin Clad Axial Temperature Distribution During a Surge Line Break in a VVER-440 Model V213

ANALYSIS APPENDIX E
NONSEISMIC STRUCTURAL RESPONSE ANALYSIS

TABLE OF CONTENTS

	Page
1.0 Introduction	E-1
2.0 Localization Compartment Response to Overpressure	E-1
2.1 Description	E-1
2.1.1 Geometry	E-1
2.1.2 Material Properties	E-2
2.2 Numerical Analysis	E-3
2.2.1 Reinforced Concrete Model	E-3
2.2.2 Three-Dimensional Structural Model	E-4
2.2.3 Results	E-5
2.3 Discussion	E-6
3.0 VVER-440 Steam Generator Vessel Response to Pressurization	E-7
3.1 Finite Element Model	E-7
3.2 Results	E-8
3.2.1 Static Pressurization	E-8
3.2.2 Dynamic Pressurization	E-9
4.0 Reactor Shaft Response to Overpressure	E-9
4.1 Finite Element Model	E-9
4.2 Static Pressurization	E-11
References	E-12

TABLES

1. **Thicknesses of VVER-440 Accident Localization System Structural Components Used in Finite Element Models**
2. **Reinforcing Bar Details for VVER-440 Model V213**
3. **Reinforcing Bar Details for VVER-440 Model V230**
4. **Material Properties of M-400 Concrete**
5. **Class A-III Reinforcing Steel Material Properties**
6. **Liner Material Properties (22K Steel)**
7. **Observations of Localization Compartment Response to Overpressurization (VVER-440 Model V213)**
8. **Observations of Localization Compartment Response to Overpressurization (VVER-440 Model V230)**
9. **Low Alloy Carbon Steel Materials**
10. **Weights of the Floor Module**
11. **Observations of the Reactor Shaft Wall Response to Overpressurization (VVER-440 Model V230)**
12. **Observations of the Reactor Shaft Wall Response to Overpressurization (VVER-440 Model V213)**
13. **Observations of the Reactor Shaft Wall Response to Overpressurization (VVER-1000 Model V320 Thick Wall)**
14. **Observations of the Reactor Shaft Wall Response to Overpressurization (VVER-1000 Model V320 Thin Wall)**
15. **Observations of the Reactor Shaft Floor Response to Overpressurization (VVER-1000 Model V320)**

FIGURES

1. Reactor Building with Localization Compartment for a VVER-440 Model V213
2. Localization Compartment and Substructure for a VVER-440 Model V213
3. Pump Room, Cable Corridor and Refueling Pond for a VVER-440 Model V213
4. Layout of Reinforcing Bars and Liner of a Typical Section of the Floor, Ceiling, or Wall of the ALS.
5. Reactor Building with Localization Compartment for a VVER-440 Model V230
6. Localization Compartment and Substructure for a VVER-440 Model V230
7. Pump Room, Cable Corridor and Refueling Pond for a VVER-440 Model V230
8. Representation of Layers of Individual Rebars as Layers of Smeared Rebars
9. Finite Element Model of the Localization Compartment and Substructure VVER-440 Model V213
10. Finite Element Model of the Pump Room, Cable Corridor, Lower Shaft, and Diagonal Corner Walls VVER-440 Model V213
11. Finite Element Model of the Localization Compartment Substructure VVER-440 Model V213
12. Layout of Reinforcing Steel in the Steam Generator Compartment Floor VVER-440 Model V213
13. Layout of Reinforcing Steel in the Diagonal Corner Walls VVER-440 Model V213
14. Layout of Reinforcing Steel in the Steam Generator Compartment Walls and Substructure Outer Walls VVER-440 Model V213
15. Finite Element Model of the Localization Compartment VVER-440 Model 230
16. Finite Element Model of the Pump Room, Cable Corridor, Lower Shaft, and Diagonal Corner Wall VVER-440 Model V230
17. Deformed Shape of Localization Compartment for VVER-440 Model V213 at an Overpressure of 0.40 Megapascal (58.0 pounds per square inch; magnification 10X)
18. Side View of Deformed Localization Compartment for VVER-440 Model V213 at an Overpressure of 0.40 Megapascal (58.0 pounds per square inch; magnification 10X)
19. Top View of Deformed Localization Compartment for VVER-440 Model V213 at an Overpressure of 0.40 Megapascal (58.0 pounds per square inch; magnification 10X)
20. View from Bubbler Tower of Deformed Localization Compartment to VVER-440 Model V213 at an Overpressure of 0.40 Megapascal (58.0 pounds per square inch; magnification 10X)

21. Deformed Shape of Pump Room, Cable Corridor, and Diagonal Corner Walls for VVER-440 Model V213 at an Overpressure of 0.40 Megapascal (58.0 pounds per square inch; magnification 10X)
22. Deformed Shape of Localization Compartment for VVER-440 Model V230 at an Overpressure of 0.30 Megapascal (43.5 pounds per square inch; magnification 10X)
23. Side View of Deformed Localization Compartment for VVER-440 Model V230 at an Overpressure of 0.30 Megapascal (43.5 pounds per square inch; magnification 10X)
24. Top View of Deformed Localization Compartment for VVER-440 Model V230 at an Overpressure of 0.30 Megapascal (43.5 pounds per square inch; magnification 10X)
25. View from Bubbler Tower of Deformed Localization Compartment for VVER-440 Model V230 at an Overpressure of 0.30 Megapascal (43.5 pounds per square inch; magnification 10X)
26. Deformed Shape of Pump Room, Cable Corridor, and Diagonal Corner Walls for VVER-440 Model V230 at an Overpressure of 0.30 Megapascal (43.5 pounds per square inch; magnification 10X)
27. Ceiling Displacement at Node 51 (figure 5) Versus Overpressure for VVER-440 Model V213
28. Ceiling Displacement at Node 51 (figure 5) Versus Overpressure for VVER-440 Model V230
29. Liner Yield Stress Versus Overpressure at Side Wall-Floor Junction for VVER-440 Model V213
30. Liner Yield Stress Versus Overpressure at Turbine Building Wall-Steam Generator Compartment Ceiling at Element 51 for VVER-440 Model V213 (figure 5)
31. Liner Yield Stress Versus Overpressure in Ceiling at Element 719 for VVER-440 Model V213 (figure 5)
32. Stresses (pounds per square inch) in Ceiling Reinforcing Bars at an Overpressure of 0.40 Megapascal (58.0 pounds per square inch)
33. Stresses (pounds per square inch) in Turbine Building Wall and Side Wall Reinforcing Bars at an Overpressure of 0.40 Megapascal (58.0 pounds per square inch) for VVER-440 Model V213
34. Liner Yield Stress Versus Overpressure at Side Wall-Floor Junction for Model V230
35. Liner Yield Stress Versus Overpressure at Turbine Building Wall-Steam Generator Compartment Ceiling at Element 51 for VVER-440 Model V230 (figure 5)
36. VVER-440 Steam Generator Shell Configuration
37. Finite Element Nodalization of Axisymmetric Conical Shell Mode
38. Effective Plastic Strain Along Inner and Outer Surface of the Shell Model for 4.7 Megapascals (682 pounds per square inch) Loading
39. Effective Plastic Strain Along Inner and Outer Surface of the Shell Model for 5.7 Megapascals (827 pounds per square inch) Loading

40. Effective Plastic Strain Along Inner and Outer Surface of the Shell Model for 9.5 Megapascals (1378 pounds per square inch) Loading
41. Effective Plastic Strain Along Inner and Outer Surface of the Shell Model for 12.5 Megapascals (1813 pounds per square inch) Loading
42. Effective Plastic Strain Along Inner and Outer Surface of the Shell Model for 16.4 Megapascals (2378 pounds per square inch) Loading
43. Axial Displacement of Semi-Elliptical Cap Apex (Node 1)
44. Radial Displacement at Cylinder Mid-Height (Node 42)
45. Effective Plastic Strain at Semi-Elliptical Cap Apex (Element 1 at Node 1)
46. Effective Plastic Strain at Semi-Elliptical Cap Corner (Element 21 at Node 22)
47. Effective Plastic Strain at Cylinder Mid-Height (Element 42 at Node 42)
48. Effective Plastic Strain at Cylinder Bottom (Element 48 at Node 49)
49. Deformed Configuration of Model at 12.5 Megapascals (1813 pounds per square inch) Loading
50. Deformed Configuration of Model at 16.4 Megapascals (2378 pounds per square inch) Loading
51. Effective Plastic Strain Along Inner and Outer Surface of the Shell Model at Dynamic Equilibrium for 9.5 Megapascals (1378 pounds per square inch) Dynamic Loading
52. Effective Plastic Strain Along Inner and Outer Surface of the Shell Model at Dynamic Equilibrium for 12.5 Megapascals (1813 pounds per square inch) Dynamic Loading
53. Axial Displacement of Semi-Elliptical Cap Apex (Node 1) for 9.5 Megapascals (1378 pounds per square inch) Dynamic Loading
54. Effective Plastic Strain at Semi-Elliptical Cap Apex (Element 1 at Node 1) for 9.5 Megapascals (1378 pounds per square inch) Dynamic Loading
55. Effective Plastic Strain at Semi-Elliptical Cap Corner (Element 21 at Node 22) for 9.5 Megapascals (1378 pounds per square inch) Dynamic Loading
56. Axial Displacement of Semi-Elliptical Cap Apex (Node 1) for 12.5 Megapascals (1813 pounds per square inch) Dynamic Loading
57. Effective Plastic Strain at Semi-Elliptical Cap Apex (Element 1 at Node 1) for 12.5 Megapascals (1813 pounds per square inch) Dynamic Loading
58. Effective Plastic Strain at Semi-Elliptical Cap Corner (Element 21 at Node 22) for 12.5 Megapascals (1813 pounds per square inch) Dynamic Loading
59. Elevation View of the VVER-1000 Model V320 Reactor Building

- 60. **Cross-Sectional View of the VVER Model V320 at 19.3 Meter Elevation**
- 61. **Finite Element Mesh for the Reactor Shaft Wall**
- 62. **Finite Element Mesh for the Reactor Floor Support**
- 63. **a) Plan View and b) Photograph of the Reinforcing for the VVER-1000 Model V320 Reactor Floor Support**

ANALYSIS APPENDIX E

NONSEISMIC STRUCTURAL RESPONSE ANALYSIS

1.0 Introduction

This appendix provides analyses of three VVER pressure boundaries subjected to overpressure due to hypothetical and postulated events. The scope of the analyses is to determine pressure levels at which structural damage would result in loss of the pressure boundary. Numerical models of these boundaries are developed and examined under accident loading conditions.

The first boundaries analyzed are the VVER-440 model V213 and model V230 localization compartments. Three-dimensional finite element models of the structures were made and statically loaded with internal pressure. The location of several failure sites and associated pressure levels were identified. The second analysis dealt with the steam generator vessel response to static and dynamic internal pressure loading. The failure pressure and mode are determined with an axisymmetric finite element model of the vessel. The final boundary considered is the reinforced concrete reactor shaft. The use of a simple axisymmetric model identified the failure pressure for both the VVER-440 model V213 and model V230 designs. A simple axisymmetric model of the reactor shaft is also used for the VVER-1000 model V320. In addition, a circular plate type of finite element model is used to analyze the floor of the reactor shaft of the VVER-1000 model V320 design.

2.0 Localization Compartment Response to Overpressure

The NEPTUNE¹ finite element computer program was used to evaluate the structural capacity of the localization compartment subjected to overpressure during severe accidents. A complex three-dimensional finite element model was developed. The model represents the major structural components of the compartment: steam generator compartment, pump room, reactor shaft, cable corridor, refueling pond, and substructure walls. The reinforcing steel and liner are explicitly accounted for within the concrete wall/slab element. The nonlinear behavior of the steel as well as the cracking of the concrete is modelled. The interior boundary of the localization compartment is pressurized until failure. Finite element models are developed for both designs.

2.1 Description

This section describes the geometry and material properties of the structural materials used in VVER-440 accident localization compartments. The relevant details of the plant that are needed for a structural analysis are highlighted.

2.1.1 Geometry

Figure 1 shows the reactor building for a VVER-440 model V213 module. Figure 2 shows the localization compartment, which consists of the steam generator room and the lower part of the reactor shaft. The pump room, upper shaft (refueling pit), and substructure are also shown in the figure. The pump room, cable corridor, and refueling pond are shown in figure 3; the diagonal corner walls of the steam generator room are also visible. The steam generator room is a rectangular box-like reinforced concrete structure, with the following outside dimensions: 48 meters (157.5 feet) long by 39 meters (128.0 feet) wide and 16 meters (52.5 feet) high. The concrete ceiling is 1.5 meters (4.9 feet) thick and contains two adjacent layers of reinforcing bars near its inside and outside faces. The bars within adjacent layers form an orthogonal grid pattern. The ceiling contains several 8.5 meters (27.9 feet) diameter penetrations through which the steam generators are inserted. Steel covers are placed over the openings to seal the ceiling after the steam generators are in place. The steam generators, which weigh 233×10^3 kilograms (1.6×10^4 slugs)

each when filled with water, are suspended from the ceiling. The floor slab of the localization compartment is 3.0 meters (9.8 feet) thick and contains multiple layers of reinforcing steel.

The exterior walls of the localization compartment are 1.5 meters (4.9 feet) thick and also contain layers of rebars near their inner and outer faces. A 5.5 meters (18.0 feet) wide by 6.9 meters (22.6 feet) high opening in the wall provides a passageway to the bubbler tower. Diagonal reinforced concrete walls, which are 0.75 meters (2.5 feet) thick, are located in the corners. These walls, which extend from the floor to the ceiling, reduce the ceiling span.

The annular shaped pump room is located around the reactor shaft within the steam generator room. The outer radius of the room is 13.5 meters (44.3 feet), and the reinforced concrete walls are 0.85 meters (2.8 feet) thick. It should be noted that the primary coolant pumps, which are in the pump room, are supported on tripod support frames that rest on the steam generator floor. Thus, the pumps and their support assemblies pass through openings in the pump room floor. The annular space between the pump assemblies and the openings is filled with biological shielding that is one-fourth the thickness of the pump room floor. Thus, the pump room floor is not tightly sealed from the steam generator room.

The reactor shaft, which extends from the basemat to the ceiling of the steam generator room, provides support to both the floor and ceiling of the steam generator room. The shaft has an inner diameter of about 3.3 meters (10.8 feet) and a thickness of about 2.25 meters (7.38 feet). It is reinforced with multilayers of reinforcing steel. Rectangular openings in the shaft wall allow passage of the inlet and outlet pipes. The clearance gap between the coolant pipes and the openings will allow steam and air to flow between the lower shaft and the steam generator compartment. The shaft is encased in a metal liner. The shaft is the inner boundary for the pump room.

The floor of the steam generator compartment is supported by the substructure. The outer walls of the steam generator room continue downward to the basemat and provide support to the outer edge of the steam generator compartment. In addition, there are several interior walls below the floor of the steam generator room that provide additional support. This lowest level provides space for safety related equipment. The thickness of the ALS structures assumed in the analysis of the VVER-440 model 213 are given in table 1 and the details of the reinforcing bars are summarized in table 2. Figure 4 is a schematic that is used with table 2 to locate the rebars within the concrete slab.

Figure 5 shows the reactor building for a VVER-440 model V230 module. Figure 6 shows the localization compartment, and figure 7 shows the pump room, cable corridor, fuel storage pool and a single diagonal corner wall. Since there is a large similarity between the models V213 and models V230, only the significant structural differences will be discussed. First, the VVER-440 model V230 does not have a substructure; the floor slab of the steam generator room is the basemat. Second, one of the corners on the refueling pond side does not have a diagonal corner wall. Third, the steam generator room is smaller; it is 42 meters (137.5 feet) long (outside dimensions) by 39 meters (128.0 feet) wide and 11 meters (36.1 feet) high. Fourth, the walls and floor and ceiling slabs are thinner. The walls of the steam generator compartment and the ceiling slab are 1.0 meters (3.3 feet) thick and the corner diagonal walls are 0.5 meters (1.6 feet) thick. Table 1 summarizes the thicknesses of the concrete structure of the VVER-440 model 230 and table 3 contains the details of the reinforcing bars.

2.1.2 Material Properties

The concrete used to construct the localization compartment is taken to be design brand M-400 which has a compressive strength of 400 kilograms-force per square centimeter (5,689 pounds per square inch). The yield strength is taken to be 200 kilograms-force per square centimeter (2,845 pounds per square inch), which is one-half of the compressive strength. For most concretes, a typical value of the ultimate strain is 0.3 percent; this value is used herein. Young's modulus is 302×10^3 kilograms-force per square centimeter (4.3×10^6 pounds per square inch) and Poisson's ratio is 0.2. The tensile strength of concrete ranges from 0.05 to 0.10 times the compressive strength. The higher value is

achieved in a laboratory environment while the lower value is typical of in situ measurements. Here the lower value is used, and the tensile strength is 20 kilograms-force per square centimeter (284.5 pounds per square inch). Table 4 summarizes the material data^{2,3} for M-400 concrete.

The reinforcing steel in the localization compartment is class A-III deformed bars with diameters of 20 millimeters (0.79 inches) and 32 millimeters (1.26 inches). The modulus of elasticity of the bars is 200 gigapascals (29×10^6 pounds per square inch), the yield point is 400 megapascals (58×10^3 pounds per square inch), the ultimate strength is 600 megapascals (87×10^3 pounds per square inch), and the ultimate strain is 19 percent. The rebar properties are summarized in table 5.

The 6 millimeter (0.24 inch) thick liner is fabricated from low carbon steel assumed to have properties similar to the Soviet carbon steel⁴ designated as 22K. The material properties are as follows: Young's modulus of 207 gigapascals (30×10^6 pounds per square inch), Poisson's ratio of 0.3, yield stress of 265 megapascals (38×10^3 pounds per square inch), ultimate strength equal to 431 megapascals (62×10^3 pounds per square inch), and an ultimate strain of 15.5 percent. A summary of the liner properties⁴ is presented in table 6.

Over the timescales during which the pressure loadings attain their peak values (Analysis Appendix C), heatup of the structural walls due to condensation and convective heat transfer is limited to a shallow layer near the surface. Accordingly, the effects of variations in material properties with temperature were not significant and were therefore not modeled in the calculations.

2.2 Numerical Analysis

This section describes the numerical model of the VVER-440 accident localization compartments and the computational predictions of their response to overpressure. The first subsection gives a detailed description of the model that is used to represent lined, reinforced concrete walls and slabs of the structure. The next subsection describes the complex finite element model that is used to represent the major structural details of the components of the localization compartment. The final subsection gives results of the structural response of VVER-440 models V213 and V230 to beyond design basis pressure loads.

2.2.1 Reinforced Concrete Model

A quadrilateral Mindlin plate element is used to represent the lined, reinforced concrete walls and slabs of the localization compartment and substructure. The element has the capability to model arbitrarily positioned reinforcing bars within the wall/slab thickness and to model the membrane response of a steel liner on the inner surface of a wall/slab. The nonlinear material model for concrete can simulate both compressive and tensile behavior. The strain hardening part of the behavior is modelled with a von Mises loading function, and failure is detected with the Hsieh-Ting-Chen four-parameter model. The post-failure response of concrete in the tension regime (tension softening) follows an element-size-independent cracking criterion. The energy release rate is used as a strain-softening parameter to control the cracking behavior. To account for the orthotropic softening in the element after cracking, an orthotropic stiffness matrix is employed. Compression softening is modelled through the von Mises loading function in which an isotropic stiffness matrix is used.

The rebars are represented in the element by discrete layers of smeared reinforcing steel (figure 8). Within the element, individual layers are used to represent corresponding layers of actual reinforcement. The following geometric information is required for each layer of rebar: the distance from the centroidal plane, the angle that the bars make relative to side 1 of the element, the bar's cross-sectional area and the spacing (pitch) between bars. The stress in the bars in each layer is assumed to be uniaxial and to lie along the length of the bar; bending of the bars is not considered. A uniaxial elasto-plastic constitutive relationship is used to compute the stress from the strain along the bar direction.

The discrete-bar stress is converted into an equivalent smeared reinforcing layer stress. It should be noted that the reinforcement is taken to be rigidly bonded to the concrete; debonding of rebar and concrete is not taken into account.

The liner is represented by a thin layer located on the inner surface. Only the membrane response of the liner is taken into account, and a state of plane stress is assumed. A two-dimensional elasto-plastic strain hardening material model with a von Mises yield criterion is used to represent the liner steel. The liner is assumed to be bonded to the concrete throughout the simulation.

2.2.2 Three-Dimensional Structural Model

A complex finite element model for the localization compartment of a VVER-440 model V213 and its substructure has been developed. Because of the near symmetry of this structure along a vertical plane from the turbine building to the bubbler tower, a model that included one-half of the localization compartment is sufficient. The model includes representations for the steam generator compartment, the corner walls, the pump room, the cable corridor, the reactor shaft, the refueling pond, and the substructure walls. Figure 9 is a perspective view of the complete model with the hidden lines removed. The ceiling, bubbler tower wall, reactor shaft, refueling pond, pump room, cable corridor and substructure are clearly visible. Depicted in figure 10 are the lower shaft, pump room, cable corridor, corner walls, and steam generator room floor. The localization compartment substructure is visible in figure 11.

The reinforcing steel is explicitly modelled within the concrete wall/slab model. The arrangement of the reinforcing steel is shown for some of the key structural components in figures 12 to 14. The orthogonal grid of rebars in the floor is depicted in figure 12. It should be noted that the orientation of the rebars is correctly shown in the figures; however, for the sake of clarity, only one rebar per direction is shown in each element. The pitch, which is the distance between rebars, is input into the code to assure that the correct number of bars participate in the response. Figure 13 shows the bar layout for the diagonal corner walls, and figure 14 illustrates the orientation within the walls of the steam generator compartment and substructure outer walls.

The model is subjected to fixed boundary conditions (i.e. no translations or rotations allowed) at the basemat, which is the location where all the substructure walls and reactor shaft end. There is a vertical symmetry plane that runs from the wall adjacent to the turbine building to the wall adjacent to the bubbler tower. This plane bisects the cable corridor, pump room, reactor shaft, and refueling pond. The nodes that lie along this plane are allowed to translate in the x- and z-directions but not in the y-direction. Only rotations about the y-axis are permitted. Since the bubbler tower is not explicitly modelled here, it is assumed that its effect is to constrain the ceiling from motion in the x-direction. All the remaining nodes in the model are unconstrained.

Because of a time constraint, a new finite element model for the model V230 design could not be developed. However, the finite element model of the model V213 design was modified to account for most of the major differences between the two designs. The substructure and diagonal corner wall at the refueling pond end were decoupled from the model V213 mesh. All wall and slab thicknesses were changed to the model V230 specifications. However, the length, width, and height of the steam generator room was kept the same. Since there is no substructure the nodes of the floor slab of the steam generator room were restrained from motion. Figure 15 is a perspective view of the complete model, and figure 16 shows the model for the pump room, cable corridor, lower shaft, and diagonal corner wall.

The load is a pressure applied to the interior surfaces of all the walls that belong to the interior boundary of the localization compartment. The following structural components are loaded: ceiling, floor, inner diameter of reactor shaft up to the bellows, side walls, and back wall. Since the pump room is not sealed from the steam generator room, it will be subjected to the same pressure as the steam generator room. Thus, the floor and walls of the pump room have identical pressures on their inside and outside surfaces that results in a net pressure load of zero. In contrast, the pump room ceiling has a pressure applied to it. The same arguments apply to the corner walls; thus, the corner walls are not

loaded, but the ceiling within the wall is loaded. Since the bubbler tower is subjected to the same overpressure as the steam generator room, there is no loading applied to the wall between the steam generator compartment and bubbler tower. No loads are applied to the substructure.

The finite element model is subjected to two loading sequences. In the first sequence, the model is loaded to failure by applying load increments of 0.05 megapascal. In the second sequence, the model is first loaded to a pressure of 0.21 megapascal absolute (30.5 pounds per square inch absolute) and then subjected to a vacuum of 0.075 megapascal absolute (10.88 pounds per square inch absolute).

2.2.3 Results

The structural response of the localization compartment subjected to the above loading histories was obtained with the NEPTUNE code. The first analysis was performed to obtain an estimate of the failure pressure of the localization compartment. Incremental load steps of 0.05 megapascal were applied to the model.

The deformed shapes of the localization compartment and related structures for the VVER-440 model V213 design are shown in figures 17 to 21. From figure 17 it is seen that large displacements occur in the ceiling, pump room, and turbine building wall. Figures 18 and 20 indicate the largest vertical displacement of the ceiling occurs between the pump room wall and side wall; note, it is closer to the turbine building wall than the bubbler tower wall. The pump room seems to stiffen the ceiling above it. The deformed shape of the turbine building wall is depicted in figures 18 and 19; the largest displacement occurs in the region of the vertical leg of the cable corridor. A comparison between the horizontal movement of the ceiling and floor (figure 18) shows that the ceiling stretches more. The bulging of the side wall is clearly shown in figures 19 and 20. Deformations of the substructure are relatively small. The deformed shapes of the pump room, cable corridor, and diagonal corner walls are illustrated in figure 21.

Deformed structure plots for the VVER-440 model V230 design are shown in figures 22 to 26. The major difference in response between the two designs is that for the VVER-440 model V230 design the largest vertical displacement in the ceiling occurs near the corner that does not have a diagonal corner wall under it. The largest bulge for the side wall is also shifted toward that corner (figure 24). The deformation pattern of the turbine room wall has a maximum horizontal displacement that is shifted off center (figure 24).

For the VVER-440 model V213 design, the displacement history of node 51 (figure 9), which is the point of maximum displacement in the ceiling, is shown in figure 27. The displacement appears to be linear up to an overpressure of 0.1 megapascal (0.2 megapascal absolute, 29 pounds per square inch absolute) and has a magnitude of 0.1 centimeters (0.04 inches). Nonlinear response is evident beyond 0.1 megapascal (0.2 megapascal absolute, 29 pounds per square inch absolute), and the displacement increases to 4.0 centimeters (1.6 inches) at 0.4 megapascal gauge (0.5 megapascal absolute, 72.5 pounds per square inch absolute). The nonlinear part of the curve reflects the increasing degree of concrete cracking throughout the structure. For the VVER-440 model V230 design, the response is somewhat similar; however, a comparison between figures 27 and 28 show that for the same overpressure the VVER-440 model V230 displacements are significantly higher. Maximum ceiling slab displacement occurs at node 53 (figure 15), and its history is presented in figure 28.

The variation of the yield stress with overpressure at several locations in the liner of the VVER-440 model V213 design were examined to identify areas of high stresses. The history of the yield stress for the liner of element 259, which is located at the side wall-floor junction between the two diagonal corner walls, is depicted in figure 29. The liner is seen to remain elastic up to an overpressure of 0.25 megapascal (0.35 megapascal absolute, 50.8 pounds per square inch absolute), and then responds plastically at the higher pressures. Figure 30 shows a similar response occurs for the liner at the junction between turbine building wall and steam generator compartment ceiling (element 59 in figure 6). Figure

31 shows that the ceiling (element 719 in figure 6) liner responds elastically indicating that liner failure in this region has a low probability.

The stresses in the reinforcing bars of the VVER-440 model V213 design were examined to locate areas for potential failure. In figure 32 the tensile stresses in the first layer of rebar in the ceiling are displayed. The largest value occurs at the turbine room wall in the area of the cable corridor. This has been identified as a potential failure location. Rebar rupture would be caused by a combination of high tensile stress in combination with a high shear stress. Figure 33 shows the tensile stresses in the rebar that are in the wall adjacent to the turbine room and in the side wall. It is seen that large tensile stresses occur at the junction of the floor and the wall adjacent to the turbine room and also at the junction of the floor and side wall. The combination of tensile and shear loads would also cause the rebar to fail at these locations. Once these rebar, the remaining rebar in the section could not take the additional load and the entire section would fail.

The histories of the yield stress for the VVER-440 model V230 design are shown in figures 34 and 35. Liner yielding is seen to start at an overpressure of 0.225 megapascal (0.325 megapascal absolute, 47.1 pounds per square inch absolute). Cracking starts in the ceiling slab and walls at an overpressure of 0.05 megapascal (0.15 megapascal absolute, 21.8 pounds per square inch absolute) and becomes extensive by 0.30 megapascal absolute (43.6 pounds per square inch).

An examination of the cracking occurring in the VVER-440 model V213 design during this loading sequence was performed by studying the digital output. Concrete cracking was first noticed at an overpressure of 0.25 megapascal absolute (36.3 pounds per square inch absolute). Cracking occurred in the surface at the top of the corner walls, the bottom of the side wall, and the ceiling near the vertical cable corridor. Also, the pump room walls began to crack. Up to the 0.10 megapascal (0.20 megapascal absolute, 29.0 pounds per square inch absolute) overpressure level, the global structural response appeared to be linear. Nonlinear global structural response became evident after 0.10 megapascal (0.20 megapascal absolute, 29.0 pounds per square inch absolute) as additional areas began to crack and as the cracks progressed thru the thickness of the wall. At 0.35 megapascal gauge (0.45 megapascal absolute, 65.3 pounds per square inch absolute), a crack exists thru 80 percent of the wall thickness at the side wall-floor junction. Note, this is a location of high rebar stress. At 0.40 megapascal (0.50 megapascal absolute, 72.5 pounds per square inch absolute), a thru crack exists at the wall-ceiling junction near the cable corridor, which is a region of high rebar stress.

The second loading sequence was an initial overpressurization to 0.11 megapascal (0.21 megapascal absolute, 30.5 pounds per square inch absolute) and then a decrease to a vacuum of 0.025 megapascal (0.075 megapascal absolute, 10.9 pounds per square inch absolute). The results indicate that the localization compartment responds elastically and no cracks appear in the concrete.

2.3 Discussion

For the VVER-440 model V213, the structure responds elastically up to an overpressure of 0.10 megapascal (0.20 megapascal absolute, 29.0 pounds per square inch absolute). During the 0.10-0.15 megapascal (0.20-0.25 megapascal absolute, 29.0-36.3 pounds per square inch absolute) overpressure interval, localized cracking is observed at the following locations: top of the corner walls, bottom of the side wall, ceiling near the vertical walls and reactor shaft, vertical cable corridor, and pump room walls. At a global level, the structure starts to behave nonlinearly at an overpressure of 0.10 megapascal (0.20 megapascal absolute, 29.0 pounds per square inch absolute) as the cracking progresses over larger regions of the walls and ceiling as well as through the thickness. The liner at the side wall-floor junction and at the ceiling-vertical cable corridor wall yield at an overpressure of 0.25 megapascal (0.35 megapascal absolute, 50.8 pounds per square inch absolute). The rebar at the side wall-floor junction yields at 0.35 megapascal (0.45 megapascal absolute, 65.3 pounds per square inch absolute), and the crack at this junction has penetrated to a depth of 80 percent the wall thickness. When overpressure increases to 0.40 megapascal (0.50 megapascal absolute, 72.5 pounds per square inch

absolute), a possible rebar failure at the side wall-floor junction can occur due to combined tensile axial stress and transverse shear stress that exceed the ultimate stress of the rebars. A thru crack that can cause failure of the rebar exists at the turbine room wall-ceiling junction near the cable corridor.

Once a rebar fails, the remaining rebar and liner cannot carry the load, and they also fail. Thus, at this location a leak path would exist through both the cracked liner and the cracked concrete. Table 7 summarizes the observations on the structural behavior of the localization compartment during overpressure to ultimate capacity, which occurs at about 0.40 megapascal (0.50 megapascal absolute, 72.5 pounds per square inch absolute).

Table 8 summarizes the observations for the model V230 design. The ultimate capacity appears to be between 0.20-0.30 megapascal (0.30-0.40 megapascal absolute, 43.5-58.0 pounds per square inch absolute).

Since information was not available on localized design details, the above global analysis did not take into account the fact that these details could give rise to stress or strain concentrations that could potentially lower the ultimate capacity. For example, thick anchor plates attached to the thin liner plate could cause such concentrations. The effect of the welded joints of the liner was not taken into account. The method of liner plate anchorage to the concrete is another detail that was not considered. The details of the penetration in the ceiling thru which the steam generators are inserted and the cover with its bolting system for this penetration could effect the ultimate capacity.

3.0 VVER-440 Steam Generator Vessel Response to Pressurization

The implicit time integration version of the finite element computer program STRAW^{5,6,7} was used to analyze the static and transient response of the VVER-440 steam generator shell subjected to mechanical loadings arising from postulated accident scenarios. The analysis involves assessing the potential damage (inelastic response) of the steam generator shell due to specific static and dynamic internal pressurization. For static loading, the failure pressure and mechanism was also addressed.

An axisymmetric analysis of the steam generator shell was performed. The analysis accounted for material nonlinearities and geometric nonlinearities arising from large deflections. An axisymmetric conical shell element from the flexural element group was employed to model the steam generator shell. The cross section of the element is assumed to be homogeneous and the thickness is assumed to be constant in the entire cross section. The element is initially straight.

3.1 Finite Element Model

The steam generator shell (figure 36) is primarily cylindrical with semi-elliptical caps on each end. The cylindrical component is 10.52 meters (414.2 inches) in total length with an internal diameter of 3.2 meters (126.0 inches); it is composed of three 3.42 meter (134.6 inches) sections with 0.13 meter (5.1 inches) transition pieces. The central section is 0.13 meters (5.12 inches) in thickness; the two adjacent sections have a thickness of 0.065 meters (2.56 inches). The semi-elliptical cap has an internal minor radius of 0.3625 meter (14.27 inches) with a wall thickness of 0.065 meters (2.56 inches). A low alloy carbon steel⁴ was used to construct the steam generator shell. The material properties are given in table 9.

To assess the inelastic response of the steam generator shell, it was assumed to be rotationally and mid-height symmetric. All penetrations in the shell were neglected. Therefore, only one-half of the shell was modeled with axisymmetric conical shell elements. The model has a total of 58 elements and 59 nodes; the nodalization is shown in figure 37. The model consists of the following: 24 elements representing the semi-elliptical cap, 24 elements representing the thinner cylindrical section, one element representing the transition piece, and nine elements representing the thicker central cylindrical section. Finer discretization was employed in areas of the cap and cylinder where high stress concentrations

were thought to possibly exist. Five integration points are used for the integration through the conical shell thickness; two points are used along the length.

3.2 Results

This subsection describes (1) the static response of the steam generator vessel to selected static pressure loadings, and (2) the dynamic simulation of the vessel to two transient pressure histories.

3.2.1 Static Pressurization

Potential damage (inelastic response) of the steam generator shell is sought for four different static pressure loadings: the nominal secondary system operating pressure of 4.7 megapascals absolute (682 pounds per square inch absolute), a pressure of 5.7 megapascals absolute (827 pounds per square inch) representing a 1.0 megapascal (145 pounds per square inch) overpressure, the estimated pressure of 9.5 megapascals absolute (1378 pounds per square inch absolute) when the steam line is not vented (Analysis Appendix C) and the nominal primary system operating pressure of 12.5 megapascals absolute (1813 pounds per square inch absolute). The failure pressure and the failure mechanism of the model was also pursued.

Effective plastic strain profiles for the four static loadings are shown in figures 38 to 41. Zero arc length is the apex of the semi-elliptical cap. Results are given for both inner and outer surfaces. The point of maximum effective plastic strain for all cases is in the inside corner (element 21 at node 22) of the semi-elliptical cap. The maximum effective plastic strain values are: 0.18 percent for 4.7 megapascals (682 pounds per square inch), 0.46 percent for 5.7 megapascals (827 pounds per square inch), 5.0 percent for 9.5 megapascals (1378 pounds per square inch) and 10.0 percent for 12.5 megapascals (1813 pounds per square inch). These values are all well below the maximum strain value of 22.0 percent. Plastic deformation is predicted without failure.

The predicted static failure pressure for this model is 16.4 megapascals (2,378 pounds per square inch). The point of failure is near the mid-height (element 42 at node 42) of the 0.065 meter (2.56 inch) thick cylindrical section. High membrane circumferential strains indicate the cylinder will split open along its length. As the static load was increased to failure, the bending strains in the semi-elliptical cap, which were predominate through much of the loading, become dominated by the membrane strains. The membrane response becomes the dominant response in the model as the loading approaches its failure value. Since the cylinder is the weaker component, from a membrane point of view, failure occurred in the cylinder. If the bending strains in the semi-elliptical cap had remained dominate, the cap would have split open in the corner along the full circumference. An effective plastic strain profile, just prior to failure, is given in figure 42.

Displacement and effective plastic strain responses as a function of pressure for selected points are given in figures 43 to 48. Figures 43 and 44 give the axial displacement of the cap apex (node 1) and the radial displacement at the mid-height of the 0.065 meter (2.56 inch) thick cylinder (node 42), respectively. Figures 45 to E-48 show the variations with pressure of the effective plastic strains at the cap apex, cap corner, cylinder mid-height and cylinder bottom (adjacent to transition section), respectively. These results provide an indication of the nonlinear response during the pressurization.

Deformed configurations of the model are given in figures 49 and 50 for overpressure loading of 12.5 megapascals (1813 pounds per square inch) and 16.4 megapascals (2378 pounds per square inch), respectively. These two figures clearly show how the deformation mode changes between the 12.5 megapascals (1813 pounds per square inch) pressure load and the failure pressure load of 16.4 megapascals (2378 pounds per square inch).

3.2.2 Dynamic Pressurization

Simulations with two dynamic pressure histories were performed to evaluate the dynamic effects over the static pressurizations. The pressure-time history for the estimated overpressure when the steam line is not vented (discussed in Analysis Appendix C) was input to the calculation. Specifically, the loading condition is: 8.15 megapascals (1182 pounds per square inch absolute) at 0.2 s, 9.0 megapascals (1305 pounds per square inch) at 0.4 s, 9.5 megapascals (1378 pounds per square inch) at 0.6 s and 9.5 megapascals (1378 pounds per square inch) held constant thereafter. For a pressure loading scenario representative of pressurization to the nominal primary system pressure, the pressure-time history is: 10.7 megapascals (1552 pounds per square inch) at 0.2 s, 11.8 megapascals (1711 pounds per square inch) at 0.4 s, 12.5 megapascals (1813 pounds per square inch) at 0.6 s and 12.5 megapascals (1813 pounds per square inch) for further time.

Figures 51 and 52 yield the dynamic equilibrium results for the effective plastic strain profiles for the 9.5 megapascals (1378 pounds per square inch) and 12.5 megapascals (1813 pounds per square inch) loadings, respectively. When these are compared with the static results given in figures 40 and 41, it is seen that the dynamic results are very similar to the static results. Only in the semi-elliptical cap corner region is there some dynamic overshoot, approximately 15 percent for the 9.5 megapascals (1378 pounds per square inch) loading and 10 percent for the 12.5 megapascals (1813 pounds per square inch) loading.

The axial displacement of the cap apex, and the effective plastic strain at the cap apex and cap corner are given in figures 53 to 54, respectively, for the 9.5 megapascals (1378 pounds per square inch) dynamic loading. Figures 56 to 58 provide these results for the 12.5 megapascals (1813 pounds per square inch) dynamic loading. Their equilibrium results are compared with the static results of figures 43, 45, and 46. As stated in the previous paragraph, the dynamic results are nearly identical to the static results with a minor variance in the cap corner.

4.0 Reactor Shaft Response to Overpressure

The TEMP-STRESS^{8,9} finite element computer program was used to analyze the static response of the VVER-440 (models V213 and V230, see figures 2 and 6, respectively) and the VVER-1000 (model V320, see figures 59 and 60) reactor shafts subjected to mechanical loadings arising from an accident scenario. This computer code was developed for analyses of reinforced concrete structures subjected to thermo-mechanical loading stemming from operational as well as off-normal conditions.^{10,11} The analysis involves assessing the potential damage of the reactor shaft to a specific static internal pressurization and estimating the failure pressure.

A two-dimensional axisymmetric analysis of the reinforced concrete shafts were performed. The analysis accounted for both material and geometric nonlinearities arising from large deflections. Concrete was represented by axisymmetric continuum or flexural shell elements and the rebar by discrete hoop or membrane shell elements.

4.1 Finite Element Model

The reactor shafts are primarily steel-lined reinforced concrete cylinders with end caps and penetrations throughout. The concrete floor is the lower end cap and the steel dome is the upper cap. A bellows type seal is located just above the inlet and outlet piping, which is about mid-height in the shaft. These shafts were analyzed for pressurization in the lower region, which is bounded by the bottom end cap and the bellows. All the reactor shafts were investigated for wall failure in the free field. For the analysis of the reactor shafts it was assumed that the bellows and lower end cap will remain intact during the accident. An additional analysis of the floor (bottom end cap) in the VVER-1000 was needed, because failure here would breach the localization boundary.

Since failure of the shaft walls will occur away from the end caps, the reactor shafts were modeled as infinitely long cylinders in order to provide a lower bound solution. The cylindrical dimensions for the VVER-440 model V230 are an inner radius of 315 centimeters (124.0 inches) and an outer radius of 620 centimeters (244.1 inches). The VVER-440 model V213 has an inner radius of 325 centimeters (128.0 inches) and an outer radius of 550 centimeters (216.5 inches). In the VVER-1000 model V320 the shaft wall thickness varies significantly, as indicated in figure 59. Therefore, two analyses of the wall were required. Dimensions of the VVER-1000 Model V320 (thick wall) are an inner radius of 372 centimeters (146.5 inches) and an outer radius of 640 centimeters (252.0 inches); and the thin wall dimensions are an inner radius of 372 centimeters (146.5 inches) and an outer radius of 490 centimeters (192.9 inches). Rebar configuration for the cylinders are assumed to be the same with an interior steel liner of 6 millimeter (0.24 inch) thickness. Only the hoop rebars need to be included in the analysis, and the distribution is given in figure 61. Material properties^{2,3} of the rebar steel are given in table 5, liner steel properties⁴ are given in table 6, and the concrete material properties^{2,3} are given in table 4.

To assess the response of the reactor shaft walls, it was assumed that failure would occur due to hoop stresses. All penetrations in the shaft were neglected. The finite element model (figure 61) has a total of 17 elements and 18 nodes, with imposed boundary conditions for radial movement only, as shown. The model consists of the following: 8 elements representing the concrete, 8 elements representing the hoop reinforcement, and one membrane element for the liner. Single point integration is used for all the elements. Note that the concrete is subjected to a three-dimensional state of stress, whereas the reinforcement is in a one-dimensional state of stress. The liner is subjected to hoop and axial stresses.

Analysis of the VVER-1000 model V320 floor was investigated for a failure mechanism which is primarily due to bending stresses. The floor was modeled as a circular plate with an outer 600 centimeter (236.2 inches) radius (lower wall location), simply supported, and 200 centimeter (78.7 inch) thick. The assumed thickness is lower than the value of 240 centimeters (94.5 inches) reported by the Soviets. A thicker floor would be expected to withstand higher pressures than those for which failure is predicted here. Note that the shaft liner and concrete layer between the liner and the floor were not included in the analysis. Since the reactor shaft is built after the supporting floor, the bond between these two surfaces is questionable. So when the floor bends, the shaft liner and the concrete layer will contribute little or no resistance. The simply supported boundary condition was assumed in order to give a lower bound value of the ultimate load capacity. In reality the boundary condition is somewhere between fixed and simply supported; during the pressurization the boundary condition will change (i.e., towards simply supported) because the upper wall is undergoing increasing damage. For simplification of the analysis, the floor and wall were modeled separately.

Modeling of the floor consisted of 24 elements and 28 nodes of which 21 were translational and 7 were rotational as indicated in figure 62. Six flexural shell elements with 10 integrations (two lengthwise and five through the depth) represented the concrete, and 12 membrane elements (two integrations lengthwise only) were used for the two orthogonal rebar meshes [32 millimeter (1.3 inch) diameter bars spaced at 25 centimeters (9.8 inches) and support-I beams, sized as W8X31 sections with an area of 59 square centimeters (9.1 square inches)] on top and bottom of the floor slab. An additional six membrane elements were used to model the 6 millimeter (0.24 inch) liner attached to the bottom of the floor slab. The sizes of the rebars, supporting beams, and liner were estimated from the photograph in figure 63b.¹² As a final adjustment for the supporting beam size (cross-sectional area), the total weight of the floor without concrete was calculated (using a mass density of 7850 kilograms per cubic meter (490 pounds per cubic foot) for steel) and equivalenced with the measured weights given in table 10, where the section numbers are illustrated in figure 63a. In order to accomplish this, the area of the supporting beams were reduced from 59 square centimeters (9.1 square inches) to 55 square centimeters (8.5 square inches). The three membrane layers are attached to the concrete shell elements by a rigid connection. This type of connection provides the correct bending resistance for the membrane elements through the translation and rotation of the shell nodes.

4.2 Static Pressurization

Potential damage (inelastic response) of the reactor shaft is sought for the estimated overpressures given in Analysis Appendix C. The numerical studies indicate that failure of the shafts can be simulated successfully by assuming a quasi-static solution. Equilibrium is guaranteed by a dual criterion of force balance and displacement change. Force balance (error between external and internal forces) was set at 2% and change in displacement at the nodes was set to 0.1%. In all cases, equilibrium was achieved up to the load step just before failure or collapse of the structure. The load step used for all the simulations was 0.1 megapascal (14.5 pounds per square inch). Also, the failure pressure and failure mechanism of the models were also analyzed.

The reactor shafts of the VVER-440, models V230 and V213, were subjected to a maximum internal overpressure of 2.6 megapascal (2.7 megapascal absolute, 392 pounds per square inch absolute). Results of the finite element analysis for both reactor shafts are given in tables 11 and 12, respectively. The progression of failure in both cases is about the same. After an elastic response, the concrete cracks in the hoop direction and is followed by the liner and hoop rebar yielding. After yielding has occurred, the liner and rebar behave plastically until the innermost rebar ruptures at an overpressure of 2.9 megapascal (3.0 megapascal absolute, 435 pounds per square inch absolute) for both shafts. Then a longitudinal rip would result in the liner and release the internal overpressure. Although the failure pressure is 0.3 megapascal (44 pounds per square inch) (about 10 percent) above the accident pressure, the reactor shafts still could fail due to uncertainties in material properties and imperfections.

A simple check of the computer analysis can be obtained through a closed form solution. Assuming a thick wall cylinder for concrete and a thin wall cylinder for steel, along with strain compatibility in the steel and concrete, an estimate can be obtained. Failure of the concrete due to hoop cracking would occur at an internal overpressure of 1.23 megapascal (1.33 megapascal absolute, 193 pounds per square inch absolute) for the VVER-440 model V230 and 1.02 megapascal (1.12 megapascal absolute, 162 pounds per square inch absolute) for the VVER-440 model V213. After cracking has occurred, the inner hoop rebar would fail at an internal overpressure of 2.47 megapascal (2.57 megapascal absolute, 373 pounds per square inch absolute) for the VVER-440 model V230 and 2.51 megapascal (2.61 megapascal absolute, 379 pounds per square inch absolute) for the VVER-440 model V213. These results indicate pressures slightly less (about 14%) than the computer results. This would be expected because the closed form solutions only utilized the ultimate strength values of the steel and did not take into account the strain energy capacity of the plastic response. However, these results give a reasonable check of the finite element results.

The reactor shaft of the VVER-1000 model V320 was subjected to a maximum internal overpressure of 4.0 megapascal (4.1 megapascal absolute, 595 pounds per square inch absolute). Results of the finite element analysis for the shaft walls are given in table 13 and 14 for the thick and thin wall sections, respectively. The progression of failure in both cases is similar to the VVER-440 results discussed earlier. It is most certain that an overpressure of 4.0 megapascal (4.1 megapascal absolute, 595 pounds per square inch absolute) would fail the shaft wall, since the minimum failure overpressure was estimated to be 2.8 megapascal (2.9 megapascal absolute, 421 pounds per square inch absolute). As discussed earlier, an investigation of the floor was required and the result of the finite analysis is given in table 15. As seen from the results, the floor would not collapse under the accident overpressure of 4.0 megapascal (4.1 megapascal absolute, 595 pounds per square inch absolute). Since the shaft wall fails at 2.8 megapascal (2.9 megapascal absolute, 421 pounds per square inch absolute) and releases the internal overpressure into the surrounding compartments, this would probably be the maximum pressure that the floor is subjected to. At this pressure only the bottom floor liner would experience some yielding, while the rebar in the floor would remain elastic.

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**Table 1 Thicknesses of VVER-440 ALS Structural
Components Used in Finite Element Models**

ALS Structural Component	Model V230	Model V213
<hr/>		
Steam Generator Compartment Floor Slab	Not used	3.0 meters
Steam Generator Compartment Ceiling Slab	1.0 meters	1.5 meters
Steam Generator Compartment Outer Walls	1.0 meters	1.5 meters
Steam Generator Compartment Diagonal Walls	0.5 meters	0.75 meters
Pump Deck Walls	0.75 meters	0.75 meters
Pump Deck Floor	1.0 meters	1.0 meters

Table 2 Reinforcing Bar Details for VVER-440 Model V213

ALS Structuring	Layer Number	Orientation*	Size (millimeters)	Z _{ref} (centimeters)
<hr/>				
Steam Generator Compartment Ceiling Slab	1	TB-BT	20	-70.0
	2	CR-TA	32	-67.4
	3	CR-TA	32	67.4
	4	TB-BT	20	70.0
Steam Generator Compartment Floor Slab	1	TB-BT	20	-145.0
	2	CR-TA	32	-142.4
	3	TB-BT	20	-139.7
	4	TB-BT	20	-70.0
	5	CR-TA	32	-67.4
	6	TB-BT	20	-64.7
	7	TB-BT	20	64.7
	8	CR-TA	32	67.4
	9	TB-BT	20	70.0
	10	TB-BT	20	139.7
	11	CR-TA	32	142.4
	12	TB-BT	20	145.0
Steam Generator Compartment Out Walls	1	H	20	-70.0
	2	V	32	-67.4
	3	V	32	67.4
	4	H	20	70.0
Steam Generator Compartment Diagonal Walls	1	H	20	-32.5
	2	V	32	-29.9
	3	V	32	29.9
	4	H	20	32.5
Pump Deck Walls	1	TB-BT	20	-32.5
	2	CR-TA	32	-29.9
	3	CR-TA	32	29.9
	4	TB-BT	20	32.5
Pump Deck Floor	1	TB-BT	20	-45.0
	2	CR-TA	32	-42.4
	3	TB-BT	20	-39.1
	4	TB-BT	20	39.7
	5	CR-TA	32	42.4
	6	TB-BT	20	45.0

*Key: CR-TA = Control Room - Transport Aisle
TB-BT = Turbogenerator Building - Bubbler Tower
H = Horizontal
V = Vertical

Table 3 Reinforcing Bar Details for VVER-440 Model V230

ALS Structure	Layer	Orientation*	Size	Z_{ref}
Steam Generator Compartment Ceiling Slab	1	TB-VC	20	-45.0
	2	CR-TA	32	-42.4
	3	CR-TA	32	42.4
	4	TB-VC	32	45.0
Steam Generator Compartment Outer Walls	1	H	20	-45.0
	2	V	32	-42.4
	3	V	32	42.4
	4	H	20	45.0
Steam Generator Compartment Diagonal Walls	1	H	20	-32.5
	2	V	32	-29.9
	3	V	32	29.9
	4	H	20	32.5
Pump Deck Walls	1	H	20	-32.5
	2	V	32	-29.9
	3	V	32	29.9
	4	H	20	32.5
Pump Deck Floor	1	TB-VC	20	-45.0
	2	CR-TA	20	-42.4
	3	TB-VC	20	-39.7
	4	TB-VC	20	39.7
	5	CR-TA	20	42.4
	6	TB-VC	20	45.0

***Key:** CR-TA = Control Room - Transport Aisle
TB-VC = Turbogenerator Building - Ventillation Center
H = Horizontal
V = Vertical

Table 4 Material Properties of M-400 Concrete

Young's modulus	302 x 10³ 29.6 4.3 x 10⁶	kilograms-force per square centimeter gigapascals pounds per square inch
Poisson's ratio	0.2	
Yield strength	200 19.6 2845	kilograms-force per square centimeter megapascals pounds per square inch
Compressive strength	400 39.2 5689	kilograms-force per square centimeter megapascals pounds per square inch
Ultimate strain	0.3	percent
Tensile strength	20 2.0 285	kilograms-force per square centimeter megapascals pounds per square inch

Table 5 Class A-III Reinforcing Steel Material Properties

Young's modulus	200 29×10^6	gigapascals pounds per square inch
Yield stress	400 58×10^3	megapascals pounds per square inch
Ultimate strength	600 87×10^3	megapascals pounds per square inch
Ultimate strain	14	percent

Table 6 Liner Material Properties (22K Steel)

Young's modulus	207 30 x 10⁶	gigapascals pounds per square inch
Poisson's ratio	0.3	
Yield stress	265 38 x 10³	megapascals pounds per square inch
Ultimate strength	431 62 x 10³	megapascals pounds per square inch
Ultimate strain	22	percent

**Table 7 Observations of Localization Compartment Response
to Overpressurization (VVER-440 Model V213)**

Pressure megapascal (pounds per square inch) guage	Observation
0.1 (14.5)	Linear elastic behavior.
0.15 (21.8)	Localized cracking starts at top of corner walls, bottom of side wall, ceiling near the vertical walls and reactor shaft, and vertical cable corridor. Pump room walls start to crack. Nonlinear structural response begins due to cracking progressing thru the depth of the walls and slabs.
0.25 (36.3)	Liner at the back wall-floor junction and at the ceiling-vertical cable corridor wall exceeds yield stress.
0.35 (50.8)	Rebars yield at back wall-floor junction. Crack at junction is 80 percent thru the wall thickness.
0.40 (58.0)	Possible rebar failure at side wall-floor junction due to combined tension and shear loading. Crack at junction is completely thru the wall thickness. Potential leakage caused by liner tear if concrete dislocates. A thru crack exists at the wall-ceiling junction near the vertical cable corridor causing rebar failure due to combined tension and shear loading.

Note: Design overpressure = 0.15 megapascal (21.8 pounds per square inch) gauge

**Table 8 Observations of Localization Compartment Response
to Overpressurization (VVER-440 Model V230)**

Pressure megapascal (pounds per square inch) gauge	Observation
0.025 (3.63)	Linear elastic behavior.
0.050 (7.25)	Cracking starts in ceiling slab, side wall, back wall, and diagonal corner wall. Nonlinear global behavior. No cracking is detected in the turbine room wall.
0.075-0.175 (10.90-25.40)	Extensive cracking occurring throughout the structure.
0.20 (29.00)	Rebars in ceiling slab near corner reach yield stress. Concrete wall has multiple cracks: one thru crack and a second crack that is 80 percent of the thickness. Potential failure due to leakage caused by liner tear if concrete dislocates. Rebar may fail by dowel action.
0.225 (32.60)	Liner at side wall-floor slab junction reaches yields stress.
0.250 (36.36)	Rebars at side wall-floor slab junction reach yield stress. Concrete wall is cracked through 80 percent of its thickness. Rebar may fail due to dowel action.
0.300 (43.50)	Concrete wall at side wall-floor slab junction has a thru crack. Potential failure due to leakage caused by liner tear if concrete dislocates; rebars may fail by dowel action.

Note: Design overpressure = 0.10 megapascal (14.5 pounds per square inch) gauge.

Table 9 Low Alloy Carbon Steel Materials

Density	7780 15.1	kilograms per cubic meter slugs per cubic foot
Poisson's ratio	0.30	
Young's modulus	189.5 27.5×10^6	gigapascals pounds per square inch
Yield stress	265 38.4×10^3	megapascals pounds per square inch
Ultimate strain	22	percent
Ultimate stress	431 62.5×10^3	megapascals pounds per square inch

Table 10 Weights of the Floor Module

	Section						
	1	2	3	4	5	6	Total
<hr/>							
Weight							
metric tonnes	163	338	257	171	239	248	1416
(tons)	(181)	(376)	(286)	(190)	(266)	(276)	(1573)

**Table 11 Observations of the Reactor Shaft Wall Response
to Overpressurization (VVER-440 Model V230)**

Pressure megapascal (pounds per square inch) gauge	Observation
1.4 (203)	Linear elastic behavior.
1.5 (218)	Initialization of hoop cracks in the concrete which propagate from inner to outer radius.
1.6 (232)	Hoop cracks finish propagating and concrete is completely cracked in the hoop direction. Yielding of the liner begins.
1.7 (247)	Yielding of the inner hoop rebars begins.
2.3 (334)	Yielding of all the hoop bars has occurred.
2.9 (421)	Innermost hoop rebar rupture, and then the liner rips open releasing the internal pressure.

Note: Design overpressure = 0.10 megapascal (14.5 pounds per square inch) gauge.

**Table 12 Observations of the Reactor Shaft Wall Response
to Overpressurization (VVER-440 Model V213)**

Pressure megapascal (pounds per square inch) gauge	Observation
<hr/>	
1.1 (160)	Linear elastic behavior.
1.2 (174)	Initialization of hoop cracks in the concrete which propagate from inner to outer radius.
1.3 (189)	Hoop cracks finish propagating and concrete is completely cracked in the hoop direction.
	Yielding of the liner begins.
1.7 (247)	Yielding of the inner hoop rebars begins.
2.2 (319)	Yielding of all the hoop bars has occurred.
2.9 (421)	Innermost hoop rebar rupture, and then the liner rips open releasing the internal pressure.

Note: Design overpressure = 0.15 megapascal (21.8 pounds per square inch) gauge.

**Table 13 Observations of the Reactor Shaft Wall Response
to Overpressurization (VVER-1000 Model V320 Thick Wall)**

Pressure megapascal (pounds per square inch) gauge	Observation
1.3 (189)	Linear elastic behavior
1.4 (203)	Initialization of hoop cracks in the concrete which propagate from inner to outer radius.
1.5 (218)	Hoop cracks finish propagating and concrete is completely cracked in the hoop direction. Yielding of the liner begins.
1.7 (247)	Yielding of the inner hoop rebars begins.
2.1 (305)	Yielding of all the hoop bars has occurred.
2.8 (406)	Innermost hoop rebar rupture, and then the liner rips open releasing the internal pressure.

Note: Design overpressure = 0.10 megapascal (14.5 pounds per square inch) gauge.

**Table 14 Observations of the Reactor Shaft Wall Response
to Overpressurization (VVER-1000 Model V320 Thin Wall)**

Pressure megapascal (pounds per square inch) gauge	Observation
0.8 (116)	Linear elastic behavior
0.9 (131)	Initialization of hoop cracks in the concrete which propagate from inner to outer radius.
1.4 (203)	Yielding of the liner begins.
1.9 (275)	Yielding of the inner hoop rebars begins.
2.1 (305)	Yielding of all the hoop bars has occurred.
3.0 (435)	Innermost hoop rebar rupture, and then the liner rips open releasing the internal pressure.

Note: Design overpressure = 0.10 megapascal (14.5 pounds per square inch) gauge.

**Table 15 Observations of the Reactor Shaft Floor Response
to Overpressurization (VVER-1000 Model V320)**

Pressure megapascal (pounds per square inch) gauge	Observation
0.4 (58)	Linear elastic behavior.
0.5 (73)	Initialization of radial and hoop cracks at bottom center of the floor.
0.5-3.4 (73-493)	Radial and hoop cracks propagate through approximately 3/5 of the floor depth and 1/2 the span length.
1.8 (261)	Bottom liner plate begins to yield at the center of the floor and propagates outward.
3.4 (493)	Bottom rebar layer begins to yield at the center of the floor and propagates outward.
4.2 (609)	Top rebar layer begins to yield at the center of the floor and propagates outward.
4.8 (696)	Concrete starts to crush at upper center of the floor, bottom layer rebar and liner rupture which causes the floor to collapse.

Note: Design overpressure = 0.10 megapascal (14.5 pounds per square inch) gauge.

Figure 1 Reactor Building with Localization Compartment for a VVER-440 Model V213

Figure 2 Localization Compartment and Substructure for a VVER-440 Model V213

Figure 3 Pump Room, Cable Corridor and Refueling Pond or a VVER-440 Model V213

- $\frac{W}{2}$ -- Width of Concrete section
- Z_{RB} -- Distance From Centerline to Reinforcing Bar
- Z_L -- Distance From Centerline to Liner

Figure 4 Layout of reinforcing Bars and Liner of a Typical Section of the Floor, Ceiling, or Wall of the Accident Localization System in VVER-440s

Figure 5 Reactor Building with Localization Compartment for a VVER-440 Model V230

Figure 6 Localization Compartment and Substructure for a VVER-440 Model V230

Figure 7 Pump Room, Cable Corridor, and Refueling Pond for a VVER-440 Model V230

Figure 8 Representation of Layers of Individual Rebars as Layers of Smeared Rebars

Figure 9 Finite Model of the Localization Compartment and Substructure (VVER-440 Model V2I3)

**Figure 10 Finite Element Model of the Pump Room, Cable Corridor,
Lower Shaft, and Diagonal Corner Walls (VVER-440 Model V213)**

Figure 11 Finite Element Model of the Localization Compartment Substructure (VVER-440 Model V213)

**Figure 12 Layout of Reinforcing Steel in the Steam Generator
Compartment Floor (VVER-440 Model V213)**

**Figure 13 Layout of Reinforcing Steel in the
Diagonal Corner Walls (VVER-440 Model V213)**

**Figure 14 Layout of Reinforcing Steel in the Steam Generator
Compartment Walls and Substructure Outer Walls (VVER-440 Model V213)**

Figure 15 Finite Element Model of the Localization Compartment
(VVER-440 Model 230)

**Figure 16 Finite Element Model of the Pump Room, Cable Corridor,
Lower Shaft, and Diagonal Corner Wall (VVER-440 Model V230)**

**Figure 17 Deformed Shape of Localization Compartment
for VVER-440 Model V2L3 at an Overpressure of 0.40 Megapascal
(58.0 pounds per square inch; magnification 10X)**

**Figure 18 Side View of Deformed Localization Compartment
for VVER-440 Model V213 at an Overpressure of 0.40 Megapascal
(58.0 pounds per square inch; magnification 10X)**

**Figure 19 Top View of Deformed Localization Compartment
for VVER-440 Model V213 at an Overpressure of 0.40 Megapascal
(58.0 pounds per square inch; magnification 10X)**

**Figure 20 View from Bubbler Tower of Deformed Localization
Compartment to VVER-440 Model V213 at an Overpressure of 0.40 Megapascal
(58.0 pounds per square inch; magnification 10X)**

Figure 21 Deformed Shape of Pump Room, Cable Corridor, and Diagonal Corner Walls for VVER-440 Model V213 at an Overpressure of 0.40 Megapascal (58.0 pounds per square inch; magnification 10X)

**Figure 22 Deformed Shape of Localization Compartment for
VVER-440 Model V230 at an Overpressure of 0.30 Megapascal
(43.5 pounds per square inch; magnification 10X)**

**Figure 23 Side View of Deformed Localization Compartment
for VVER-440 Model V230 at an Overpressure of 0.30 Megapascal
(43.5 pounds per square inch; magnification 10X)**

**Figure 24 Top View of Deformed Localization Compartment
for VVER-440 Model V230 at an Overpressure of 0.30 Megapascal
(43.5 pounds per square inch; magnification 10X)**

**Figure 25 View from Bubbler Tower of Deformed Localization Compartment
for VVER-440 Model V230 at an Overpressure of 0.30 Megapascal
(43.5 pounds per square inch; magnification 10X)**

Figure 26 Deformed Shape of Pump Room, Cable Corridor, and Diagonal Corner Walls for VVER-440 Model VZ30 at an Overpressure of 0.30 Megapascal (43.5 pounds per square inch; magnification 10X)

Displacement History

Run No.: NE7282J for Node #51

**Figure 27 Ceiling Displacement at Node 51 (figure E-5)
Versus Overpressure for VVER-440 Model V213**

Displacement History

Run No.: NE7282L for Node #51

**Figure 28 Ceiling Displacement at Node 51 (figure E-5)
Versus Overpressure for VVER-440 Model V230**

Liner Yield Stress History

Run No.: NE7282J for Element #259

Figure 29 Liner Yield Stress Versus Overpressure at Side Wall-Floor Junction for VVER-440 Model V213

Liner Yield Stress History

Run No.: NE7282J for Element #059

**Figure 30 Liner Yield Stress Versus Overpressure
at Turbine Building Wall-Steam Generator Compartment
Ceiling at Element 51 for VVER-440 Model V213 (figure E-5)**

Liner Yield Stress History

Run No.: NE7282J for Element #719

**Figure 31 Liner Yield Stress Versus Overpressure in Ceiling
at Element 719 for VVER-440 Model V213 (figure E-5)**

Figure 32 Stresses (pounds per square inch) in Ceiling Reinforcing Bars at an Overpressure of 0.40 Megapascal (58.0 pounds per square inch) for VVER-440 Model V213

**Figure 33 Stresses (pounds per square inch) in Turbine Building Wall and Side Wall
Reinforcing Bars at an Overpressure of 0.40 Megapascal
(58.0 pounds per square inch) for VVER-440 Model V213**

Liner Yield Stress History

Run No.: NE7282L for Element #259

Figure 34 Liner Yield Stress Versus Overpressure at Side Wall-Floor Junction for VVER-440 Model V230

Liner Yield Stress History

Run No.: NE7282L for Element #059

Figure 35 Liner Yield Stress Versus Overpressure at Turbine Building Wall-Steam Generator Compartment Ceiling at Element 51 for VVER-440 Model V230 (figure E-5)

Figure 36 VVER-440 Steam Generator Shell Configuration

Figure 37 Finite Element Nodalization of Axisymmetric Conical Shell Model

**Figure 38 Effective Plastic Strain Along Inner and Outer Surface
of the Shell Model for 4.7 Megapascals
(680 pounds per square inch) Loading**

Figure 39 Effective Plastic Strain Along Inner and Outer Surface of the Shell Model for 5.7 Megapascals (830 pounds per square inch) Loading

Figure 40- Effective Plastic Strain Along Inner and Outer Surface of the Shell Model for 9.5 Megapascals (1380 pounds per square inch) Loading

**Figure 41 Effective Plastic Strain Along Inner and Outer Surface
of the Shell Model for 12.5 Megapascals
(1810 pounds per square inch) Loading**

Figure 42 Effective Plastic Strain Along Inner and Outer Surface of the Shell Model for 16.4 Megapascals (2380 pounds per square inch) Loading

Figure 43 Axial Displacement of Semi-Elliptical Cap Apex (Node 1)

Figure 44 Radial Displacement at Cylinder Mid-Height (Node 42)

**Figure 45 Effective Plastic Strain at Semi-Elliptical Cap Apex
(Element 1 at Node 1)**

Figure 46 Effective Plastic Strain at Semi-Elliptical Cap Corner
(Element 21 at Node 22)

**Figure 47 Effective Plastic Strain at Cylinder Mid-Height
(Element 42 at Node 42)**

**Figure 48 Effective Plastic Strain at Cylinder Bottom
(Element 48 at Node 49)**

**Figure 49 Deformed Configuration of Model at 12.5 Megapascals
(1810 pounds per square inch) Loading**

**Figure 50 Deformed Configuration of Model at 16.4 Megapascals
(2380 pounds per square inch) Loading**

Figure 51 Effective Plastic Strain Along Inner and Outer Surface of the Shell Model at Dynamic Equilibrium for 9.5 Megapascals (1380 pounds per square inch) Dynamic Loading

Figure 52 Effective Plastic Strain Along Inner and Outer Surface of the Shell Model at Dynamic Equilibrium for 12.5 Megapascals (1810 pounds per square inch) Dynamic Loading

**Figure 53 Axial Displacement of Semi-Elliptical Cap Apex (Node 1)
for 9.5 Megapascals (1380 pounds per square inch) Dynamic Loading**

**Figure 54 Effective Plastic Strain at Semi-Elliptical Cap Apex
(Element 1 at Node 1) for 9.5 Megapascals
(1380 pounds per square inch) Dynamic Loading**

**Figure 55 Effective Plastic Strain at Semi-Elliptical Cap Corner
(Element 21 at Node 22) for 9.5 Megapascals
(1380 pounds per square inch) Dynamic Loading**

**Figure 56 Axial Displacement of Semi-Elliptical Cap Apex
(Node 1) for 12.5 Megapascals
(1810 pounds per square inch) Dynamic Loading**

**Figure 57 Effective Plastic Strain at Semi-Elliptical Cap Apex
(Element 1 at Node 1) for 12.5 Megapascals
(1810 pounds per square inch) Dynamic Loading**

**Figure 58 Effective Plastic Strain at Semi-Elliptical Cap Corner
(Element 21 at Node 22) for 12.5 Megapascals
(1810 pounds per square inch) Dynamic Loading**

Figure 59 Elevation View of the VVER-1000 Model V320 Reactor Building

Figure 60 Cross-Sectional View of the VVER Model V320 at 19.3 meter Elevation

Figure 61 Finite Element Mesh for the Reactor Shaft Wall

Figure 62 Finite Element Mesh for the Reactor Floor Support

Figure 63 a) Plan View and b) Photograph of the Reinforcing for the VVER-1000 Model V320 Reactor Floor Support

ANALYSIS APPENDIX F

SEISMIC ANALYSES

TABLE OF CONTENTS

	Page
1.0 Introduction	F-1
2.0 Seismic Analysis of the VVER-440 Model V230 Main Building	F-2
2.1 Introduction	F-2
2.2 Description of the Mathematical Model	F-2
2.3 Seismic Response of the Main Building	F-2
2.4 Detailed Calculation of the Stress in the Columns for the Various Cross Sections	F-3
1) 1.5 meters x 0.6 meters (48 inches x 24 inches) Columns in the Turbine Hall	F-3
2) 1.2 meters x 0.6 meters (48 inches x 24 inches)	F-5
3) 0.8 meters x 0.6 meters (32 inches x 24 inches)	F-6
4) 0.75 meters x 0.6 meters (30 inches x 24 inches)	F-7
2.5 Elastic Seismic Capacity and Ultimate Seismic Capacity	F-8
3.0 Seismic Analysis of the VVER-1000 Main Building	F-10
3.1 Introduction	F-10
3.2 Results	F-11
3.3 Effects of Prestress and Dead Weight on the Containment Shell Prestress	F-14
3.4 Ultimate Seismic Capacity	F-15
4.0 Seismic Analysis of VVER-440 Model V230 Primary Loop	F-15
4.1 Introduction	F-15
4.2 Description of the Mathematical Model	F-16
4.3 Seismic Response of the Primary Loop Without Shock Absorber	F-17
4.4 Seismic Response of VVER-440 Model V230 Primary Loop Reinforced by Hydraulic Shock Absorbers	F-17
5.0 Seismic Analysis of the VVER-1000 Primary Loop	F-18
5.1 Introduction	F-18
5.2 Seismic Analysis of VVER-1000 Primary Loop Without Hydraulic Shock Absorbers	F-19
5.3 Seismic Analysis of the VVER-1000 Primary Loop Reinforced by Shock Absorbers	F-20
6.0 Seismic Analysis of the VVER-440 Model V230 Accident Localization Compartment	F-22
6.1 Introduction	F-22
6.2 Horizontal Response	F-22
6.3 Vertical Seismic Response of VVER-440 Model V230 Accident Localization Compartment	F-22

7.0	Seismic Analysis of VVER-440 Model V230 Deaerator Tank Support	F-22
7.1	Introduction	F-22
7.2	Seismic Analysis of VVER-440 Model V230 Deaerator Support Platform	F-23
7.3	Seismic Capacity	F-25
8.0	Seismic Analysis of the VVER-440 Model V230 Reactor Support Including Annular Water Tank	F-26
8.1	Introduction	F-26
8.2	Support Flange	F-26
8.3	Annular Water Tank	F-27
9.0	Sloshing Response in a VVER-440 Refueling Pool, Steam Generator, and Deaerator Tank	F-27
9.1	Introduction	F-27
9.2	Sloshing Wave Height in VVER-440 Model V230 Spent Fuel Storage Pool	F-27
9.3	Sloshing in the Steam Generator	F-28
9.4	Sloshing in the Deaerator Water Storage Tank	F-29
10.0	Storage Tank Integrity Assessment under Seismic Loading	F-29
10.1	Background	F-29
10.2	Storage Tanks in VVER Plants	F-30
Reference		F-30

TABLES

1. **Beam Properties**
2. **Soil Properties and Foundation Impedance**
3. **Maximum Shear Force and Bending Moment**
4. **Shear Stresses and Bending Stresses at Critical Locations**

FIGURES

1. Cross-Sectional View and Plan View of the VVER-440 Model V230 Main Building
2. Skeleton of the VVER-440 Model V230 Main Building
3. Dimensions of the Cross Section and Rebar Distribution of the Columns
4. Roof-Slab Configuration of the VVER-440 Model V230 Main Building
5. Mathematical Model of the VVER-440 Model V230 Main Building
6. Lumped Weight (pounds) at Various Locations
7. 0.3 g Ground Acceleration Used in the Seismic Analyses
8. Deformed Configuration at $t = 1$ second (amplification factor = 500)
9. Deformed Configuration at $t = 8$ second (amplification factor = 500)
10. Calculated Acceleration Response at Node 9
11. Calculated Acceleration Response at Node 11
12. Calculated Acceleration Response at Node 37
13. Calculated Acceleration Response at Node 39
14. Calculated Acceleration Response at Node 53
15. Calculated Maximum Acceleration (g) at Various Locations
16. Calculated Maximum Displacement (inches) at Various Locations
17. Calculated Maximum Bending Moment ($\times 10^6$ inch-pounds) at Various Locations
18. Calculated Maximum Axial Force ($\times 10^6$ pounds) in the Girders and Roof Truss
19. Cross-Sectional View of the Column with Dimension of 1.5 Meters x 0.6 meters
20. Cross-Sectional View of the Column with Dimension of 1.2 Meters x 0.6 meters
21. Cross-Sectional View of the Column with Dimension of 0.8 Meters x 0.6 meters
22. Cross-Sectional View of the Column with Dimension of 0.75 Meters x 0.6 meters
23. Cross-Sectional and Plan View of the VVER-1000 Main Building

24. **Plan Views at Various Elevations**
25. **Mathematical Model of the VVER-1000 Main Building**
26. **Floor Thickness at Various Elevations of the VVER-1000 Main Building**
27. **Lumped Weight at Various Locations**
28. **0.3 g Ground Input Acceleration**
29. **Enlarged Deformation Configuration**
30. **Acceleration Fourier Spectrum at Top of Containment**
31. **Acceleration Fourier Spectrum at Top of Reactor Section**
32. **Displacement at Base of Reactor Building**
33. **Displacement at Top of Reactor Section**
34. **Displacement at Top of Containment**
35. **Calculated Relative Displacement at Node 7**
36. **Calculated Relative Displacement at Node 15**
37. **Calculated Relative Displacement at Node 31**
38. **Calculated Maximum Acceleration at Various Locations**
39. **Calculated Bending Moment History of Element 1 (bottom of the building)**
40. **Calculated Shear Force of Element 1 (bottom of the building)**
41. **Calculated Bending Moment of Element 4 (bottom of reactor section)**
42. **Calculated Shear Force of Element 4 (bottom of reactor section)**
43. **Calculated Bending Moment History to Element 8 (bottom of cylindrical containment)**
44. **Calculated Shear Force of Element 8 (bottom of cylindrical containment)**
45. **Hand Calculations of the Reactor Forces**
46. **Bending Moment and Shear Forces at Critical Areas**
47. **Total Number of Rebar Rods**
48. **Concrete Block Module of the Containment**

49. Rebar Arrangement in the Containment
50. Simplified Cross Section of the Containment
51. Prestressed Concrete Containment of VVER-1000
52. Top View of Primary Loop of VVER-440 Model V230
53. Mathematical Model of VVER-440 Model V230 Primary Loop
54. Locations of the Springs Simulating the Hydraulic Shock Absorbers
55. Calculated Movement at Steam Generator
56. Maximum Lateral Movement at Various Locations
57. Calculated Acceleration Time History and Fourier Spectrum at the Steam Generator
58. Calculated Maximum Relative Acceleration, g , at Various Locations
59. Top View of the Primary Loop Reinforced by the Shock Absorbers
60. Maximum Displacement at Various Locations of the Primary Loop Reinforced by Shock Absorbers
61. Deformed Configuration (amplified 1000 times)
62. Calculated Displacement and Fourier Spectrum at Node 75
63. Top View of the Primary Loop of VVER-1000
64. Elevation View of Primary Loop of VVER-1000
65. VVER-1000 Steam Generator Supports
66. Mathematical Model of the Primary Loop of the VVER-1000
67. 0.603 g Acceleration Input Motion Used in the Analysis
68. Calculated Displacement Time History at the Steam Generator
69. Calculated Displacement Time History at Primary Loop
70. Calculated Maximum Displacements at Various Locations (with no shock absorbers)
71. Location of the Springs Simulating the Shock Absorbers
72. Calculated Maximum Displacements
73. Maximum Displacements at Various Locations

74. Accident Localization Compartment of the VVER-440 Model V230 (elevation view)
75. Isoparametric View of the Accident Localization Compartment
76. Mathematical Model of the Accident Localization Compartment
77. Vertical Ground Motion and Fourier Spectrum
78. Deformed Configuration
79. Schematic Diagram of the Deaerating Column with a Tank
80. Location of the Deaeration Tank
81. Response Spectrum of the Ground Motion
82. Cross Section of the Columns
83. Annular Water Tank of VVER-440 Model V230
84. Support Flange of the Annular Water Tank
85. Mathematical Model and Deformed Configuration of the Annular Water Tank
86. Location of the VVER-440 Model V230 Refueling Pond
87. Top View of the Refueling Pool
88. Water Levels During Normal Operation and Refueling
89. 2-D Mathematical Model of the Refueling Pool of Normal Operation (top figure) and Refueling Operation (bottom figure)
90. Calculated Sloshing Wave Height at Node 91 (normal operation)
91. Calculated Sloshing Wave Height at Node 91 (refueling condition)
92. Configuration of Steam Generator
93. Sloshing in the Deaerating Storage Tank

ANALYSIS APPENDIX F

SEISMIC ANALYSES

1.0 Introduction

This Analysis Appendix summarizes the results of the U.S. DOE Team's seismic analyses of VVER structures, components, and piping systems. Background Appendix J provides information concerning seismicity in regions where VVERs are located and information on Soviet seismic design and analysis. The reference units are the VVER-440 model V230 and the VVER-1000 model V320 units at Kozloduy in Bulgaria. The structures, components, and piping systems investigated include:

1. VVER-440 model V230 main building;
2. VVER-1000 model V320 main building;
3. VVER-440 model V230 primary cooling loop;
4. VVER-1000 model V320 primary cooling loop;
5. VVER-440 model V230 accident localization compartment;
6. VVER-440 model V230 deaerator support platform;
7. VVER-440 model V230 reactor support including annular water tank;
8. Sloshing response in VVER-440 refueling pool, steam generator and deaerator tank.

The linear time history analysis method is used. The seismic loading is the ground motion in terms of acceleration time history with the peak acceleration determined by the maximum expected seismic intensity at the Kozloduy site (i.e., eight in MSK scale). The peak horizontal ground acceleration corresponds to the magnitude of eight in the MSK scale, varying from 0.15 g to 0.2 g. In this study, the seismic loading with a peak horizontal acceleration of 0.3 g is used. Since linear analysis is carried out, the results can be scaled down or scaled up depending on the seismic loading level. For example, the results can be scaled down by a factor of three (3) if the seismic loading level is 0.1 g.

It is noted that the measured peak horizontal ground acceleration of the 1977 Vrancea earthquake at Bucharest, Romania (185 kilometers away from the epicenter) was about 0.18 g¹. The Kozloduy plant is about 350 kilometers away from the epicenter. The maximum horizontal acceleration at the Kozloduy site during the 1977 earthquake was not reported. However, the intensity assessed by specialists is in the order of 5 to 6 on the MSK scale. It can be reasonably assumed that the maximum acceleration at the Kozloduy site was about 0.1 g during the 1977 Vrancea earthquake.

In the analyses, the response of the structures, components, and piping systems is calculated to the imposed earthquake acceleration loadings. Predicted motions and stresses are employed to provide an assessment of the seismic capacity of the structures, components, and piping systems. The most critical area under seismic loading and the possible failure modes are addressed. The study is limited to linear seismic analysis. Therefore, the highest seismic stress obtained from the analysis is compared with the yield stress of the material. The comparison provides an assessment for the elastic seismic capacity (in terms of the level of the ground acceleration) of the VVER structures, components, and piping systems. The elastic seismic capacity is then multiplied by a ductility factor suggested by N. M. Newmark in 1975 for determination of the ultimate seismic capacity (or so-called "fragility") which defines the seismic loading level causing the failure of structures or components.²³

2.0 Seismic Analysis of the VVER-440 Model V230 Main Building

2.1 Introduction

The elevation and plan views of the VVER-440 model V230 main building are shown in figure 1. It comprises a turbine hall (between planes A and B in figure 1), a control and auxiliary equipment section (between planes B and C), a reactor section (between planes C and D), and an exhaust ventilation center (between planes D and E). The turbine hall houses four turbogenerators, each 220 MW. The turbine hall has forty-two (42) bays, each 6 meters long. Its total length is 252 meters. The span of the turbine hall and the reactor section are 39 meters each, whereas the span of the equipment section and exhaust ventilation center are 12 meters each. The reactor section consists of reinforced concrete compartments where the two 440-MWe reactors are located. The reactor shaft is deeply embedded. The reinforced concrete structures of the reactor section are cast in-situ from the bottom level to 10.5 or 11.8 meters elevation (see figure 1) and form a rigid box which serves as the foundation for the precast reinforced concrete skeleton.

The skeleton of the main building is made up of columns (see figure 2) with a cross section of 1.5 meters x 0.6 meters (60 inches x 24 inches) in the turbine hall, 1.2 meters x 0.6 meters in the reactor section, and 0.8 meters x 0.6 meters in the exhaust ventilation center, and girders 1.4 meters x 0.6 meters in the cross section. Note that the cross section of the columns in the turbine hall and the reactor section reduces to 0.75 meters x 0.6 meters at the location above the overhead cranes. Figure 3 shows the distribution of No. 10 steel rebar in the columns and girders. They are based on best estimates from available drawings.

The roof of the turbine hall and the reactor section consists of prestressed reinforced concrete slabs laid on metal trusses, as shown in figure 4. The member size of the roof truss is assumed to be 6 inches x 6 inches x 1/2 inches. The roofs of the equipment section and exhaust ventilation center are made of reinforced concrete slabs supported by prefabricated reinforced concrete girders. The floors in the equipment section are made of prefabricated reinforced concrete slabs resting on prefabricated reinforced concrete girders. The exterior walls of the main building are made of reinforced foam concrete panels 0.25 meters thick.

2.2 Description of the Mathematical Model

The mathematical model of the main building is shown in figure 5. It is a two-dimensional model representing a transverse cross section of the main building. The columns and girders are represented by the beam elements, whereas the roof trusses in the turbine hall and reactor section are represented by the spring elements. The girders in the equipment section are hinged-end. The box-type accident localization structure is assumed to be rigid. The mass and stiffness of the individual column, girder, and truss are lumped together in the direction perpendicular to the 2-D plane. The lumped weights are shown in figure 6. The foundation medium is represented by the soil spring and dampers. The shear wave velocity is 5000 feet/second, which is very close to the soil characteristics at the site of the Kozloduy plant. Figure 7 shows the input ground motion of 10-s duration. The maximum acceleration used in the analysis is 0.3 g. The results can be linearly scaled if the seismic loading level is increased or decreased. A linear transient time history analysis using the FLUSTR-ANL computer code was carried out. The results of the analysis are described herein.

2.3 Seismic Response of the Main Building

Figures 8 and 9 show the typical deformed configuration of the main building at $t = 1$ second and 8 second (amplification factor of 500). The exterior columns in the turbine hall have a very large deformation due to the heavy mass of the concrete exterior wall. The equipment section below the crane support location has a very

small deformation due to the rigidity of the reactor section and the girders. The part of the equipment section above the crane support location, however, has a very large deformation due to the reduced column cross section.

Figures 10 to 14 show the acceleration time history and the acceleration Fourier spectrum at the five top nodes (nodes 9, 11, 37, 39, and 53) of the model. As can be seen from those figures, the dominant mode of the main building in the lateral direction has a frequency of 2.5 Hz. The frequency is relatively low. The maximum acceleration at various locations is shown in figure 15. The maximum acceleration is 1.61 g at the crane support location of the turbine hall. The maximum relative displacement with respect to the base of the building is shown in figure 16, where the maximum relative displacement is 2.5 inches at the exterior columns of the turbine hall.

Figures 17 and 18 show the maximum bending moment and the maximum axial force of various members. The shear force is small compared to the bending moment and are not reported herein. In figure 17, the maximum bending moment is 2300×10^6 inch-pounds at the top of the interior column of the turbine hall where the roof truss is supported. The exterior columns are also subjected to a very large bending moment. The maximum axial force in the roof truss is 10×10^6 pounds which occurs at the roof truss in the reactor section (see figure 18). The axial stress in the roof truss of the turbine hall is

$$\sigma = \frac{10 \times 10^6}{530} = 18,800 \text{ pounds per square inch}$$

The axial stress in the roof truss of the reactor section is

$$\sigma = \frac{9 \times 10^6}{320} = 28,000 \text{ pounds per square inch}$$

They are small compared with the yield stress of the roof truss. The axial force in the horizontal concrete girder is negligible.

The ultimate stress and yield stress for the concrete used in the main building (i.e., M-400 concrete) are 5,600 pounds per square inch and 2800 pounds per square inch, respectively. The yield and ultimate stress for the steel rebar is 58,000 pounds per square inch and 87,000 pounds per square inch, respectively. There are four different cross sections of the columns. The detailed calculation of the compressive and tensile stress for those four cross sections due to the maximum seismic bending moment are presented in the following sections.

2.4 Detailed Calculation of the Stress in the Columns for the Various Cross Sections

1) 1.5 meters x 0.6 meters (60 inches x 24 inches) Columns in the Turbine Hall

The columns with a cross section of 1.5 m x 0.6 m are located in the turbine hall (see figure 2). The maximum bending moment of this cross section is 1400×10^6 inch-pounds (see figure 17). The cross-sectional view, strain, and stress diagrams of the column with a 1.5 m x 0.6 m cross section is shown in figure 19. The first step is to locate the neutral axis (NA). From the strain diagram (figure 19),

$$\frac{\epsilon_c}{\epsilon_s} = \frac{x}{57.5 - x}$$

by Hook's law,

$$\epsilon_c = \frac{f_c}{E_c} \quad \text{and} \quad \epsilon_s = \frac{f_s}{E_s},$$

where f_c and f_s are the stresses of concrete and rebar, respectively, and E_c and E_s are the moduli of elasticity of concrete and steel, respectively. In this study, the modulus of elasticity is 3×10^6 pounds per square inch for concrete and 30×10^6 pounds per square inch for rebar. Substituting stresses for strain in the first equation,

$$\frac{f_c}{f_s} = \frac{3 \times 10^6}{30 \times 10^6} \cdot \frac{x}{57.5 - x} = \frac{x}{10(57.5-x)} \quad (1)$$

A second expression for f_c/f_s is obtained by equating the internal compressive force (C) to the internal tensile force (T)

$$C = \frac{1}{2} f_c \cdot b \cdot x - \frac{1}{2} f_c \cdot 24 \cdot x$$

and

$$T = f_s A_s = f_s \cdot 10 \cdot 1.27 = 12.7 f_s.$$

Equating C to T gives

$$\frac{f_c}{f_s} = \frac{12.7}{12x} \quad (2)$$

Combining (1) and (2) gives

$$12x^2 + 127x - 7302.5 = 0. \quad (3)$$

Solving (3) gives

$$x = 20 \text{ inches.}$$

Since the internal moment of each column equals the external applied moment due to seismic loading

$$M = 31 \times 10^6 \text{ inch-pounds} = C \text{ (or T)} \times \text{moment arm.}$$

The internal moment of each column is obtained from the maximum bending moment divided by the number of the columns (i.e., 46 columns). The moment arm, y , or the distance from the centroid of the compressive zone to the centroid of the tension zone is (57.5 inches to 6.6 inches). Thus,

$$C = T = \frac{M}{y} = \frac{31 \times 10^6}{57.5 - 6.6} = 0.60 \times 10^6 \text{ pounds,}$$

and the concrete stress (f_c) and the rebar stress (f_s) are determined from

$$f_c = \frac{0.60 \times 10^6}{12 \times 20} = 2,500 \text{ pounds per square inch}$$

and

$$f_s = \frac{0.60 \times 10^6}{12.7} = 47,000 \text{ pounds per square inch}$$

2) 1.2 meters x 0.6 meters (48 inches x 24 inches)

The columns with a cross section of 1.2 meters x 0.6 meters are located in the reactor section (see figure 2). The maximum bending moment is 500×10^6 inch-pounds (see figure 17). The cross-sectional view, strain, and stress diagrams of the column with 1.2 meters x 0.6 meters cross section is shown in figure 20. The procedure for stress calculation is similar to that used in the 1.5 meters x 0.6 meters cross section. The location of the neutral axis is determined by the following equations:

$$\frac{\epsilon_c}{\epsilon_s} = \frac{x}{45.5 - x}$$

$$\frac{t_c}{t_s} = \frac{3 \times 10^6}{30 \times 10^6} \frac{x}{45.5 - x} = \frac{x}{10(45.5 - x)}$$

$$C = \frac{1}{2} \cdot f_c \cdot b \cdot x = \frac{1}{2} \cdot f_c \cdot 24 \cdot x = 12 f_c x$$

and

$$T = f_s A_s = f_s \cdot 8 \cdot 1.27 = 10.16 f_s$$

Since $C = T$, therefore

$$\frac{t_c}{t_s} = \frac{10.16}{12x}$$

and

$$\frac{10.16}{12x} = \frac{x}{10(45.5 - x)}$$

$$12x^2 + 10.16x - 4622 = 0$$

$$x = 19.2 \text{ inches}$$

$$C = T = \frac{M}{y} = \frac{17.8 \times 10^6}{45.5 - 6.4} = 0.45 \times 10^6 \text{ pounds.}$$

The internal moment for each column is 17.8×10^6 inch-pounds. It is obtained from the maximum bending moment divided by the number of columns (i.e., 28).

$$f_c = \frac{0.45 \times 10^6}{12 \times 19.2} = 1,970 \text{ pounds per square inch}$$

$$f_s = \frac{0.45 \times 10^6}{10.16} = 44,000 \text{ pounds per square inch.}$$

3) 0.8 meters x 0.6 meters (32 inches x 24 inches)

The columns of the cross section of 0.8 meters x 0.6 meters are located in the exhaust ventilation center (see figure 2). The cross-sectional view, strain, and stress diagrams are shown in figure 21. From figure 21,

$$\frac{\epsilon_c}{\epsilon_s} = \frac{x}{9.5 - x}$$

$$\frac{f_c}{f_s} = \frac{3 \times 10^6 \cdot x}{30 \times 10^6 (29.5 - x)} = \frac{x}{10 (29.5 - x)}$$

$$C = \frac{1}{2} f_c \cdot b \cdot x = \frac{1}{2} \cdot f_c \cdot 24 \cdot x = 12 f_c x$$

and

$$T = f_s A_s = f_s \cdot 8 \cdot 1.27 = 10.16 f_s$$

Since $C = T$, therefore,

$$\frac{f_c}{f_s} = \frac{0.61 \times 10^6}{12 x}$$

and

$$\frac{10.16}{12x} = \frac{x}{10 (29.5 - x)}$$

$$12 x^2 + 10.16 x - 2997 = 0$$

$$x = 15.4 \text{ inches}$$

The internal moment is obtained from the maximum bending moment of 17.8×10^6 inch-pounds divided by the number of columns (i.e., 28).

$$C = T = \frac{M}{y} = \frac{10 \times 10^6}{29.5 - 5.1} = 0.43 \times 10^6 \text{ pounds}$$

$$f_c = \frac{0.43 \times 10^6}{12 \times 15.4} = 2,369 \text{ pounds per square inch}$$

$$f_s = \frac{0.43 \times 10^6}{10.16} = 42,000 \text{ pounds per square inch.}$$

4) 0.75 meters x 0.6 meters (30 inches x 24 inches)

The cross section at the top of the turbine hall and the reactor section is reduced from 1.5 meters x 0.6 meters and 1.2 meters x 0.6 meters to the cross section of 0.75 meters x 0.6 meters. The reduction of the cross section is to accommodate the support for the traveling of the overhead cranes. The maximum bending moment of this cross section is 1300×10^6 inch-pounds at the top of the columns in the turbine hall (see figure 17).

$$\frac{\epsilon_c}{\epsilon_s} = \frac{x}{27.5 - x}$$

$$\frac{f_c}{f_s} = \frac{3 \times 10^6}{30 \times 10^6} = \frac{x}{(27.5-x)} = \frac{x}{10 (27.5-x)}$$

$$C = \frac{1}{2} f_c \cdot b \cdot x = \frac{1}{2} \cdot f_c \cdot 24 \cdot x = 12 f_c x$$

and

$$T = f_s A_s = f_s \cdot 8 \cdot 1.27 = 10.16 f_s$$

Since $C = T$, therefore,

$$\frac{f_c}{f_s} = \frac{10.16}{12 x}$$

and

$$\frac{10.16}{12x} = \frac{x}{10 (27.5-x)}$$

$$12 x^2 + 10.16 x - 2794 = 0$$

$$x = 14.8 \text{ inches}$$

$$C = T = \frac{M}{y} = \frac{28.2 \times 10^6}{22.5} = 1.25 \times 10^6 \text{ pounds}$$

The internal bending moment for each column is 28.2×10^6 inch-pounds. It is obtained from the maximum bending moment divided by the number of columns.

$$f_c = \frac{1.25 \times 10^6}{14.8 \times 12} = 7,065 \text{ pounds per square inch}$$

$$f_s = \frac{1.25 \times 10^6}{10.16} = 118,000 \text{ pounds per square inch.}$$

2.5 Elastic Seismic Capacity and Ultimate Seismic Capacity

The calculated seismic stresses in the columns are listed below:

Cross Section	Concrete (pounds per square inch)	Rebar (thousand pounds per square inch)
1.5 meters x 0.6 meters	2500	47
1.2 meters x 0.6 meters	1970	44
0.8 meters x 0.6 meters	2370	42
0.75 meters x 0.6 meters	7065	118

The maximum stresses (7065 pounds per square inch for concrete and 118,000 pounds per square inch for rebar) occur at the top of the interior columns (cross section 0.75 meters x 0.6 meters) in the turbine hall. The material properties for the M-400 concrete and A-III rebar used in the building are as follows:

M-400 Concrete

yield stress	2800 pounds per square inch,
ultimate stress	5600 pounds per square inch.

A-III Rebar

yield stress	58,000 pounds per square inch,
ultimate stress	87,000 pounds per square inch.

The elastic seismic capacity for the concrete and rebar are:

$$\begin{aligned}\text{elastic seismic capacity for concrete} &= \frac{2800}{7065} \times 0.3 \text{ g} = 0.118 \text{ g} \\ \text{elastic seismic capacity for rebar} &= \frac{58}{118} \times 0.3 \text{ g} = 0.15 \text{ g} .\end{aligned}$$

The concrete will reach the elastic limit first. Therefore, the ultimate seismic capacity is

$$\text{ultimate seismic capacity} = 0.118 \text{ g} \times 2.5 = 0.3 \text{ g} .$$

A ductility factor of 2.5 is used in the calculation of the ultimate seismic capacity. The ultimate seismic capacity is about twice the size of the maximum expected seismic loadings at the Kozloduy site. Therefore, the VVER-440, V230 should withstand the maximum expected seismic event at the Kozloduy site.

The criteria developed by N. M. Newmark,^{2,3} in 1975 for the selection of the ductility factor are described below. The VVER-440 main building belongs to category II structure and a ductility factor of 2.5 is selected.

Class	Description
I-S	Equipment, instruments, or components performing vital functions that must remain operative during and after earthquakes; structures that must remain elastic or nearly elastic; facilities performing a vital safety-related function that must remain functional without repair. Ductility factor = 1 to 1.3.
I	Items that must remain operative after an earthquake but need not operate during the event; structures that can deform slightly in the inelastic range; facilities that are vital but whose service can be interrupted until minor repairs are made. Ductility factor = 1.3 to 2.0.
II	Facilities, structures, equipment, instruments, or components that can deform inelastically to a moderate extent without unacceptable loss of function; structures housing items of Class I or I-S that must not be permitted to cause damage to such items by excessive deformation of the structure. Ductility factor = 2.0 to 3.0.
III	All other items which are usually governed by ordinary seismic design codes; structures requiring seismic resistance in order to be repairable after an earthquake. Ductility factor = 3.0 to 8.0, depending on material, type of construction, design of details, and control of quality.

3.0 Seismic Analysis of the VVER-1000 Main Building

3.1 Introduction

The VVER-1000 main building (see figure 23) consists primarily of a cylindrical shell, a reactor section, equipment rooms between two thick concrete mats (i.e., foundation mat and the mat at 13 meter level), and office buildings around the containment shell. Figure 24 shows some cross-sectional views of the equipment room areas (two top figures) and in the reactor section (two bottom figures). A stick model is developed (see figure 25) where elements 1, 2, and 3 represent three floors between the two concrete mats, elements 4 through 7 represent the four floors in the reactor section, and elements 8 through 15 represent the cylindrical containment shell (diameter = 45 meters, thickness = 1.2 meters). It is noted that the axis of the cylindrical containment coincides with that of the reactor section. Three sets of springs and dampers are attached to the bottom of the model representing the horizontal, vertical, and rocking stiffness and damping of the soil medium. The area and moment of inertia of each beam are shown in table 1. The floor thicknesses are shown in figure 26. Figure 27 shows the lumped mass (or weight) at each node. The FLUSTR-ANL code is used in the analysis. The soil properties and spring-damper constants are given in table 2. Soil with 4000 feet/second and 6000 feet/second of shear velocity was used in the analysis. Since the results between the two analyses are close, only the results of the 4000-feet/second shear velocity are reported herein. Note that the soil at the Kozloduy plant has a shear wave velocity of 5000 feet/second. Figure 28 shows the 0.3-g input ground motion which is identical to that used in the seismic analysis of the VVER-440 Model V230 main building. A transient time-history analysis was carried out using a 0.005-seconds time step, and a 5% critical damping for the main building was used. The results are presented herein.

3.2 Results

The seismic analysis indicated that the VVER-1000 main building has two dominant modes. They are 4.3 Hertz for the containment shell and 7.5 Hertz for the reactor section. The vibration (with an amplification factor of 400) at $t = 3$ seconds is shown in figure 29. The frequencies of the two modes are identified from the acceleration Fourier spectrum at the top of the reactor section (see figure 30) and the top of shell (see figure 31). Figures

32 through 34 are the total displacements at the bottom of the building (node 1), the top of the reactor section (node 15), and the top of the shell (node 31), respectively. Figures 35 through F-37 are the relative displacement (with respect to the bottom of the building) and the floor response spectra (with 3, 5, 7 percent equipment damping) at the mat (13-meter level), the top of the reactor section, and the top of the shell, respectively.

The maximum acceleration at various elevations are shown in figure 38. As can be seen, the acceleration at the top of the reactor section is amplified 2.56 times (i.e., 0.769 g), whereas the acceleration at the top of the shell is amplified 4.66 times (i.e., 1.40 g). The shear and moment experienced at the bottom of the building (beam 1), the bottom of the reactor section (beam 4), and the bottom of the shell (beam 8) are shown in figures 39 to 44. The maximum shear force and moment in each beam member are given in table 3. A simple hand calculation (see figure 45) on the shear forces at the bottom of the shell, the reactor section, and the building based on the accelerations at various levels agree well with the computer results.

Figure 46 shows the shear and bending moment at the three critical areas mentioned above. The tensile and shear strength of the concrete (M-400) are 500 pounds per square inch and 1000 pounds per square inch, respectively. It is conservative to assume that concrete will crack if any tensile stress exceeds the tensile strength. A simple calculation is performed herein to determine the shear and tensile stresses of the concrete at the three critical areas mentioned above.

Element 1. Bottom of the Building

$$A_1 = 1.44 \times 10^6 \text{ in}^2, \quad I_1 = 92 \times 10^{10} \text{ in}^4$$

$$\text{shear area} = \frac{1}{2} A_1$$

$$\sigma_s \text{ (shear stress)} = \frac{F}{\text{shear area}} = \frac{135 \times 10^6}{.5 \times 1.44 \times 10^6}$$

$$\begin{aligned} \sigma_b \text{ (tensile stress)} &= \frac{MY}{I} = \frac{175 \times 10^9 \times (33 \times 40)}{92 \times 10^{10}} \\ &= 250 \text{ pounds per square inch} \\ &< 500 \text{ pounds per square inch} \end{aligned}$$

Element 4. Bottom of Reactor Section

$$A_4 = 5.76 \times 10^5 \text{ inch}^2, \quad I_4 = 14.5 \times 10^{10} \text{ inch}^4$$

$$\text{shear area} = \frac{1}{2} A_4$$

$$\sigma_s = \frac{50 \times 10^6}{.5 \times 5.76 \times 10^5} = 175 \text{ pounds per square inch} < 1000 \text{ pounds per square inch}$$

$$\sigma_b = \frac{28 \times 10^9 \times 40}{14.5 \times 10^{10}} = 170 \text{ pounds per square inch} > 500 \text{ pounds per square inch}$$

Element 8, Bottom of the Containment Shell

$$A_8 = 2.6 \times 10^5 \text{ inch}^2, I_8 = 9.5 \times 10^{10} \text{ inch}^4$$

$$\text{shear area} = \frac{1}{2} A_8$$

$$\sigma_s = \frac{50 \times 10^6}{.5 \times 2.6 \times 10^5} = 384 \text{ pounds per square inch} < 1000 \text{ pounds per square inch}$$

$$\sigma_b = \frac{70 \times 10^9 \times 888}{9.5 \times 10^{10}} = 654 \text{ pounds per square inch} > 500 \text{ pounds per square inch}$$

Table 4 contains the calculated shear stresses and bending stresses at the three locations. It appears that the concrete will be cracked at the bottom of the shell since the tensile strength is exceeded. The stresses at the other two locations are below the concrete's strength. A detailed stress analysis at the bottom of the containment shell is presented herein.

Stresses at the Bottom of the Containment

Figures 47 through 49 show the rebar arrangement in the containment shell. It appears that two layers of rebar mesh are used across the wall, and the pitch of 5.3 inches is measured from figure 47. A total of 1130 vertical rebar rods are contained in half of the containment cross section. In this study, No. 11 rebar is assumed (diameter 1.41 inches, area = 1.56 square inches). Figure 50 shows the cross section of the containment, strain and stress distribution. The entire tension effect is assumed to be in three zones, as shown in figure 50 (a). Zone 1 consists of 280 #11 rebar rods, whereas Zones 2 and 3 consists of 560 and 280, respectively. The compression effect is assumed to be in two zones. The assumptions of linear strain and stress proportional to strain are shown in figure 50 (b) and (c). The first step is to locate the neutral axis (NA). From the strain diagram [figure 50(b)],

$$\frac{\epsilon_c}{\epsilon} = \frac{x}{1860-x}$$

By Hook's law,

$$\epsilon_c = \frac{f_c}{E_c} \quad \text{and} \quad \epsilon_s = \frac{f_s}{E_s},$$

where f_c and f_s are stress of concrete and rebar, respectively, and E_c and E_s are the moduli of elasticity of concrete and steel, respectively. In this study, the modulus of elasticity for the concrete is 3×10^6 pounds per square inch and for the steel is 30×10^6 pounds per square inch.

Substituting stresses for strain in the first equation

$$\frac{f_c}{f_s} = \frac{3 (10^6) x}{30 (10^6) (1860-x)} = \frac{x}{10 (1860-x)} \quad (1)$$

A second expression for f_c/f_s is obtained by equating the internal compressive force (C) to the internal tensile force (T). Thus

$$\begin{aligned}
 C &= f_c \times 48 \times 688 + 2 \times \left(\frac{1}{2} f_c \times 48 \times x\right) \\
 &= f_c (33024 + 48 x) , \\
 T &= f_s A_s = 280 f_s \times 1.56 + 2 \times (280 \times 0.75 f_s \times 1.56) \\
 &\quad + 2 \times (280 \times 0.25 f_s \times 1.56) = 1309 f_s .
 \end{aligned}$$

Equating C to T gives

$$\frac{f_c}{f_s} = \frac{1309}{33024 + 48 x} . \quad (2)$$

Combining (1) and (2) gives

$$48 x^2 + 34333 x - 243474 = 0 . \quad (3)$$

Solving (3) gives

$$x = 440 \text{ inches.}$$

Since the internal resisting moment equals the external applied moment due to seismic loading

$$M = 70 \times 10^2 \text{ in-pounds} = (C \text{ or } T) \times \text{moment arm.}$$

The moment arm, y , or the distance from the centroid of the compressive zone to the centroid of the tension zone is about 1400 inches. Thus,

$$C = T = \frac{M}{y} = \frac{70 \times 10^9}{1400} = 5 \times 10^7 \text{ pounds,}$$

and the stresses are determined from

$$\begin{aligned}
 f_c &= \frac{C}{33024 + 48 x} = \frac{5 \times 10^7}{33024 + 48 \times 440} \\
 &= 925 \text{ pounds per square inch} \\
 f_s &= \frac{5 \times 10^7}{1309} = 38,000 \text{ pounds per square inch}
 \end{aligned}$$

3.3. Effects of Prestress and Dead Weight on the Containment Shell Prestress

According to figure 51 there are 45 polyethylene ducts at an angle of 35 degrees to the horizontal. Each one is prestressed with a force of 10 MN. Therefore, the total compressive force in the vertical direction is

$$\frac{45 \times 10 \times 10^6}{4.48} \times \sin 35^\circ = 57 \times 10^6 \text{ pounds}$$

(1 pound = 4.48 Newton).

The compressive stress of the shell is

$$\frac{57 \times 10^6}{2.6 \times 10^5} = 220 \text{ pounds per square inch}$$

(area of shell = 2.6×10^5 square inch).

Dead Weight

The compressive stress due to the dead weight of the shell (i.e., 53×10^6 lb) is

$$\frac{53 \times 10^6}{2.6 \times 10^5} \times 0.5 = 100 \text{ pounds per square inch.}$$

A reduced gravitational acceleration of 0.5 g is used to account for upward seismic motion.

The total compressive stress due to prestress and dead weight is

$$220 + 100 = 320 \text{ pounds per square inch.}$$

Therefore, the net tensile stress at the bottom of the shell is

$$650 - 320 = 330 \text{ (pounds per square inch)}$$

(650 pounds per square inch obtained from seismic analysis),

which is smaller than the concrete tensile strength (i.e., 500 pounds per square inch). Therefore, a crack is not anticipated at the bottom of the containment under the 0.3-g seismic loading.

The total compressive stress due to seismic, prestress, and dead weight effect is

$$925 + 320 = 1245 \text{ pounds per square inch.}$$

The ultimate compressive strength and yield stress for the containment material (i.e., M-400 concrete) is 5600 and 2800 pounds per square inch, respectively.

3.4 Ultimate Seismic Capacity

The seismic analysis indicates that the bottom of the cylindrical containment has the highest seismic stress. The concrete compressive stress at that location is 1245 pounds per square inch, and the tensile stress in the rebar is 38,000 pounds per square inch. The highest seismic stresses are compared with the yield stress of 2800 pounds per square inch of concrete and 58,000 pounds per square inch of the A-III rebar for the calculation of the elastic seismic capacity.

$$\text{Elastic seismic capacity of concrete} = \frac{2800}{1245} \times 0.3 \text{ g} = 0.67 \text{ g}$$

$$\text{Elastic seismic capacity of rebar} = \frac{58}{38} \times 0.3 \text{ g} = 0.45 \text{ g}$$

The elastic seismic capacity is governed by the rebar. The ultimate seismic capacity is

$$0.45 \text{ g} \times 2.5 = 1.15 \text{ g},$$

which is about 8 times larger than the maximum expected seismic level at the Kozloduy site. Note that a ductility factor of 2.5 is used in the calculation of the ultimate seismic capacity.

4.0 Seismic Analysis of VVER-440 Model V230 Primary Loop

4.1 Introduction

The VVER-440 model V230 has six primary cooling loops, as shown in figure 52. Each primary loop consists of a horizontal steam generator, a primary pump, and two isolation valves, one in the hot leg and the other in the cold leg, as shown in figure 52. The pipe has an outside diameter of 560 millimeters (22 inches) and a thickness of 32 millimeters (1.25 inches). The length of the hot leg is about 14 meters. The pipe is made of austenitic steel OCr18N12T (18% Cr, 12% Ni, 1% Ti, 0.06% C) which is very similar to stainless steel 316 (AISI 316 17% Cr, 12% Ni).

In order to ensure undisturbed thermal expansion of the primary cooling loops, steam generators, pumps and pipes, both the VVER-440s and the VVER-1000s have no rigid supports. Pipelines are free to move, horizontal steam generators are suspended, and circulation pumps have movable supports. The reactor vessel is the only fixed element in the piping system. Sufficiently wide gaps and flexible packing are provided in the passages of pipes through the concrete floor separating the pump compartment from the steam generator compartment. The system of pipes is arranged so as to allow for self-compensation for the thermal expansion of pipelines.

During the March 4, 1977, Vrancea (Romania) earthquake, one of the steam generators moved a distance of 12 centimeters (about 5 inches). Since the 1977 Vrancea (Romania) earthquake, all the equipment of the primary cooling system in the Kozloduy VVER-440 unit has been reinforced by hydraulic shock absorbers. Each steam generator is reinforced by eight hydraulic shock absorbers (four in the transverse direction, four in the axial direction), each able to withstand a force of fifty (50) tonnes (1 tonne = 1000 kilograms). Each primary pump is reinforced by three hydraulic shock absorbers with a load capacity of twenty-five (25) tonnes for each shock absorber. Each isolation gate valve is reinforced by one shock absorber with a load capacity of five (5) tonnes.

4.2 Description of the Mathematical Model

Figure 53 shows the isoparametric views of the mathematical model for the seismic analysis of the primary loop. Figure 54 shows the spring elements which simulate the effects of the shock absorbers. Seismic excitation is assumed to be along the transverse direction of the hot leg which is the most unfavorable condition for the loop under seismic events. Two cases are studied: with shock absorbers and without shock absorbers. The pertinent information used in the analysis is described below:

excitation motion: - 0.3 g, 10 seconds ground acceleration time history

direction: - perpendicular to hot leg in horizontal plane

damping ratio: - 3%

pipe: -

O.D. - 560 millimeters

thickness - 32 millimeters

weight:

isolation valve - 8.75 tonnes

steam generator - 240 tonnes (including water)

pump - 70 tonnes (including water)

shock absorbers:

steam generator - 8 absorbers, @ 50-tonnes load capacity

pump - 3 absorbers, @ 25-tonnes load capacity

isolation valve - 1 absorber, @ 5-tonnes load capacity

material:

OCr8N12T austenitic steel

yield stress at 0.2% strain 30,000 pounds per square inch

ultimate stress 75,000 pounds per square inch

Since the primary loop is anchored at the reactor vessel which is supported at ground level, the postulated ground motion with a magnitude of 0.3 g (identical to that used in the building analysis) is used in the analysis. The loop is fixed at the two ends of the hot and cold legs. Rigid elements are used to represent the valves, pump, and steam generator. Two cases are studied. They are the case without shock absorbers and the case with shock absorbers. The results of these two cases are described in the following sections.

4.3 Seismic Response of the Primary Loop Without Shock Absorber

Figure 55 shows the displacement time history and the displacement Fourier spectrum along the excitation direction at the steam generator. The time history indicates that the maximum transverse movement of the steam generator is 14.5 inches. The magnitude of the ground motion is 0.3 g. If the magnitude of the ground motion is scaled down to 0.1 g, the movement of the steam generator would be about five inches, which is identical to

the measured movement of the steam generator of the Kozloduy plant during the 1977 Vrancea (Romania) earthquake. A seismic intensity of 5 to 6 (MSK scale) was assessed by specialists at the Kozloduy site for the 1977 Vrancea earthquake. The magnitude of the ground motion was about 0.1 g, based on an intensity of 5 to 6 in MSK scale. Figure 55 also indicates that the dominant frequency of the primary loop is 0.3 Hertz. It is very low due to the lack of lateral support in the loop. Figure 56 shows the deformed configuration of the loops (scaling factor = 5) and maximum displacement at various locations. Figure 57 shows the relative acceleration (with respect to reactor anchor) at the steam generator. The maximum relative acceleration is 0.44 g. The maximum relative accelerations at various locations are shown in figure 58.

The analysis indicates that the calculated shear stresses are very small compared with the bending stresses in the loop. The maximum bending stress is about 57,000 pounds per square inch at the reactor inlet nozzle of the loop. The bending stress at the reactor outlet nozzle is 43,000 pounds per square inch. They both exceed the yield stress of 30,000 pounds per square inch (ultimate stress = 75,000 pounds per square inch). The results are summarized below.

VVER-440 model V230 primary loop (without shock absorbers)

fundamental frequency	-	0.3 Hertz,
maximum displacement	-	14.5 inches (at steam generator),
highest bending stress	-	57,000 pounds per square inch (at reactor inlet nozzle).
elastic seismic capability	=	$\frac{30}{57} \times 0.3 \text{ g} = 0.157 \text{ g}$
ultimate seismic capability	=	$0.157 \text{ g} \times 1.3 = 0.2 \text{ g}.$

A ductility factor of 1.3 is applied in the calculation of the ultimate seismic capacity. The VVER-440 Model V230 primary loop should withstand the maximum expected seismic loadings (i.e., 0.15 g).

4.4 Seismic Response of VVER-440, Model V230 Primary Loop Reinforced by Hydraulic Shock Absorbers

Figure 59 shows the top view of the loop with shock absorbers. The equivalent springs representing the shock absorbers are placed in the directions parallel and perpendicular to the pipeline. Figure 60 shows the maximum displacement at various locations. The maximum displacement is 0.0984 in at the horizontal elbow between the steam generator and the pump. Figure 61 shows the deformed shape (scaling factor = 500) at t = 0.4 seconds.

There are three dominant frequencies. The portion of the pipe between the outlet nozzle and the isolation valve is dominant by the vibration at the frequency of 9.4 Hertz, whereas, the pipe between the inlet nozzle and the pump is dominated by the mode at a frequency of 10.5 Hertz. The rest of the loop is dominated by the mode of 11.3 Hertz. Figure 62 shows the displacement time history and Fourier spectrum at the horizontal elbow near the steam generator. The motion has a frequency of 11.3 Hertz. The analysis indicates that the calculated stresses in the loop are generally very low. The maximum bending stress is 3.9 thousand pounds per square inch at element 40 (the elbow below the primary pump). It is very small compared with the yield stress of 30,000

pounds per square inch. The seismic forces exerted by the springs are given below and are compared with the load capacity of the shock absorber.

	<u>Seismic Force (pounds)</u>	<u>Load Capacity (pounds)</u>	<u>Fragility</u>
Spring at hot leg isolation valve	16,183	11,000	0.2 g
Spring at steam generator	383,230	440,000	0.35 g
Spring at primary pump	116,960	110,000	0.30 g
Spring at cold leg isolation valve	8,370	11,000	0.40 g

It appears that the seismic force of the shock absorber at the hot leg isolation valve can only take ground motion with a magnitude up to 0.2 g. The shock absorbers at other locations can sustain 0.3-g ground motion. If all the shock absorbers fail, the primary loop will have the seismic stress identical to those in the previous case without shock absorbers. The results of this analysis are summarized below:

elastic seismic capability of piping	=	$\frac{30}{3.9} \times 0.3 \text{ g} = 2.3 \text{ g}$
ultimate seismic capability of piping	=	$2.3 \text{ g} \times 1.3 = 3 \text{ g}$
ultimate seismic capacity of shock absorber (support)	=	0.2 g.

5.0 Seismic Analysis of the VVER-1000 Primary Loop

5.1 Introduction

The VVER-1000 has four primary cooling loops. Each primary loop consists of a horizontal steam generator and a primary pump. There is no isolation valve in the loop. Figures 63 and 64 show the top view and elevation view of the loop. The pipe has an outside diameter of 990 millimeters (39 inches) and a wall thickness of 70 millimeters (2.75 inches). The pipe loop is made of 10GN2MFa low-alloy carbon steel (yield stress = 41 thousand pounds per square inch, ultimate stress = 70 thousand pounds per square inch at 650 F).

Like the primary loop of the VVER-440 model V230, the primary loop of the VVER-1000 has no rigid supports. Pipelines are free to move and the primary pumps have a movable support. Instead of hanging from the ceiling as in the case of the VVER-440, the steam generator of the VVER-1000 is bottom supported. Figure 65 shows the support structure of the steam generator. Its primary function is to support the dead weight of the steam generator and has little restraint to the lateral movement of the steam generator. Another difference between VVER-440 and VVER-1000 primary loops is that the former is located at the elevation near ground level, whereas the latter is located 26 meters (85 feet) above the ground. Therefore, the VVER-1000 primary loops are subjected to much higher seismic loading than those of the VVER-440. The major differences between VVER-1000 and VVER-440 primary loops are as follows:

VVER-440 model V230VVER-1000

Pipe O.D.	22 inches	39 inches
Wall thickness	1.25 inches	2.75 inches
Material:	OCr18N12T austenitic steel	SS clad carbon steel
yield stress	30,000 pounds per square inch	41,000 pounds per square inch
ultimate stress	75,000 pounds per square inch	70,000 pounds per square inch
Weight:		
Pump (including water)	70 tonnes	150 tonnes
Steam generator (including water)	240 tonnes	460 tonnes
Number of isolation valves	2	None
Elevation of the loop	near ground level	85 ft. above ground
Peak acceleration of the input excitation	0.3 g	0.603 g

5.2 Seismic Analysis of VVER-1000 Primary Loop Without Hydraulic Shock Absorbers

Similar to the seismic analysis of the VVER-440 primary loop, the VVER-1000 primary loop is analyzed under two cases with shock absorbers and without shock absorbers. The results for the case without shock absorbers are presented herein. Figure 66 shows the mathematical model of the VVER-1000 primary loop. The seismic excitation is assumed to be in the transverse direction of the hot leg. The loop is assumed to be fixed at the inlet and outlet nozzles of the reactor vessel. Figure 67 shows the 10-second duration input acceleration time history. The maximum acceleration is 0.603 g. It is obtained from the seismic analysis of the VVER-1000 reactor building under 0.3 g ground excitation.

Figures 68 and 69 reveal that the loop has a fundamental frequency of 0.9 Hz. It is three (3) times higher than that of the VVER-440 model V230 primary loop due to the larger pipe diameter (39 inches vs 22 inches) and the larger wall thickness (2.75 inches vs 1.25 inches). The maximum displacement at various locations is also shown in figure 70. The maximum displacement is 7.3 inches at the primary pump. The maximum bending stress is 45,000 pounds per square inch at the pipe near the reactor outlet nozzle. The shear stresses are negligible compared with the bending stress. The second highest bending stress is 37,000 pounds per square inch at the pipe near the reactor inlet nozzle. Note that the yield stress is 41,000 pounds per square inch and the ultimate stress is 70,000 pounds per square inch for the material of the VVER-1000 primary loop. The results are summarized below.

fundamental frequency	-	0.9 Hertz,
maximum displacement	-	7.3 inches (at primary pump),
maximum bending stress	-	45,000 pounds per square inch (at reactor outlet nozzle),

$$\text{elastic seismic capacity} = 41/45 \times 0.3 \text{ g} = 0.27 \text{ g}$$

$$\text{ultimate seismic capacity} = 0.27 \times 1.3 = 0.355 \text{ g}$$

A ductility factor of 1.3 is used to calculate the seismic capacity.

5.3 Seismic Analysis of the VVER-1000 Primary Loop Reinforced by Shock Absorbers

The steam generator of each VVER-1000 primary loop is restrained by eight shock absorbers (each 450-ton capacity), whereas the primary pump is reinforced by five shock absorbers (each 170-ton capacity). The capacities are about eight times bigger than those of VVER-440 model V230 primary loop. However, in the analysis, the capacities of the shock absorbers are assumed to have the same values as those of the VVER-440 model V230 primary loop. Figure 71 shows the locations of the spring elements which represent three shock absorbers (each 25-ton capacity) at the primary pump and eight shock absorbers (each 50-ton capacity) at the steam generator as those in the VVER-440 model V230 primary loop. The analysis indicates that the loop has two dominant frequencies at 6.8 Hertz and 8.3 Hertz, respectively. Figure 72 shows the maximum displacement at various locations. The maximum displacement is 0.60 inches. The maximum bending stress is only 16.6 thousand pounds per square inch (element 16) at the pipe near the outlet nozzle of the steam generator. The shear stresses are negligible. The maximum spring force at the shock absorbers are listed below:

	<u>Spring Force (pound)</u>		<u>Allowable Force (pound)</u>
Steam Generator			
Element 11	706,530	>	440,000
12	1,803,200	>	440,000
Pump			
Element 31	58,250	<	110,000
32	856,570	>	110,000

The spring force at element 12 (steam generator) and element 32 (pump) is much higher than the allowable load capacity. Therefore, subsequent computer runs are made in which the load capacity of the shock absorbers is doubled (50 tons for each of the absorbers of the pump, 100 tons for each of the absorbers of the steam generator), tripled and four times bigger. Figure 73 shows the maximum displacement at various locations for the case of double springs. The maximum displacement is 0.44 inches. The maximum spring force for each case is given below.

	<u>Spring Force (pound)</u>		<u>Allowable Force (pound)</u>
doubled shock capacity			
Element 11	396,100	<	880,000
12	996,300	>	880,000
Element 31	484,251	<	220,000
32	540,000	>	220,000
tripled shock capacity			
Element 11	321,600	<	1,320,000
12	827,850	<	1,320,000

Element 31	43,850	<	330,000
32	436,000	>	330,000

quadrupled shock capacity

Element 11	300,000	<	1,760,000
12	765,400	<	1,760,000
Element 31	43,000	<	440,000
32	383,300	<	440,000

The maximum bending stress is 11,000 pounds per square inch at the outlet nozzle of the steam generator (element 16) for the case of the doubled spring capacity. The maximum bending stress is 10,000 pounds per square inch and 9.5 thousand pounds per square inch for the case of the tripled and quadrupled spring capacity. They all occur at element 16. Therefore, the ultimate seismic capacity for the VVER-1000 primary loop reinforced by shock absorbers four times bigger than those used in the VVER-440, model V230 primary loop is:

$$\begin{aligned}\text{ultimate seismic capacity for piping} &= 45/9.5 \times 0.3g \times 1.3 = 1.85g \\ \text{ultimate seismic capacity for shock absorber} &= 0.3 g.\end{aligned}$$

Since the shock absorbers of VVER-1000 primary loop have the capacity eight times bigger than those of VVER-440 model V230 primary loop, the ultimate seismic capacity for VVER-1000 primary loop is expected to be up to 0.6 g.

6.0 Seismic Analysis of the VVER-440 Model V230 Accident Localization Compartment

6.1 Introduction

The accident localization compartment of a VVER-440 model V230 plant is a rectangular box-type reinforced concrete structure whose top part encloses the steam generator compartment and deck cell, as shown in figures 74 and 75. The width x length of the compartment is 39 meters x 42 meters (128 x 138 feet). The height is about 14 meters (46 feet). The ceilings are cast in situ and reinforced with rigid bar mats and reinforcing cages. The reactor is enclosed by a cylindrical shaft which extends about 7 meters (23 feet) below the base of the compartment. The reactor shaft is a massive structure. The part below the base of the compartment has a wall thickness of 3 meters (9.8 feet). The ceiling and the base has a thickness of 1.0 meters and 0.85 meters, respectively. The wall of the compartment has a thickness of 1.2 meters (4 feet).

6.2 Horizontal Response

The rectangular-shaped accident localization compartment is very rigid in the horizontal direction due to its heavy shear walls. The ceiling also eliminates the differential movement between the compartment's wall and the shaft. Therefore, the entire structure of the accident localization compartment will move as a rigid body under horizontal seismic excitation. No structural damage is expected for horizontal seismic excitation.

6.3 Vertical Seismic Response of VVER-440 Model V230 Accident Localization Compartment

The major concern in the vertical response of the compartment is the flexibility of the soil which may cause large vertical movement of the massive reactor shaft. Therefore, a mathematical model is constructed for calculating the response of the compartment under vertical seismic loading.

Figure 76 shows the mathematical model which represents 1/4 of the compartment. The model includes the two side walls, ceiling, base mat and reactor shaft. The foundation medium is represented by the springs. Figure 77 shows the vertical acceleration time history and fast fourier transform. The peak acceleration is scaled to 0.2 g. Figure 78 shows the amplified deformation configuration of the compartment. It indicates that only the ceiling is excited by the input loading. The basemat and reactor shaft remain undeformed. The largest deformation of the ceiling is at the midspan between shaft and side wall. The maximum seismic stress in the compartment is below 1,000 pounds per square inch. This indicates that the accident localization compartment is also very rigid in the vertical direction. Structural damage of the compartment should not be expected in seismic events. The ultimate seismic capacity should be over 5.0 g seismic loading.

7.0 Seismic Analysis of VVER-440 Model V230 Deaerator Tank Support

7.1 Introduction

A typical VVER-440 model V230 deaerator consists mainly of an accumulating tank for collection and storage of a certain amount of water and two deaerating columns where heating of water and deaerating of gases are performed (see figure 79). The deaerator is laid horizontally. It has a length of about 13 meters and a diameter of about 3 meters. The tank is supported by a steel frame which, in turn, is supported by a rectangular concrete platform at the elevation of 9.6 meters (see item 6 in figure 80). The platform is assumed to be supported by forty-five columns (five rows in the transverse direction and nine in the longitudinal direction). The cross section of the column is 0.4 meters x 0.4 meters. Under a seismic event, the integrity of the platform is of importance.

7.2 Seismic Analysis of VVER-440 Model V230 Deaerator Support Platform

Figure 80 shows the elevation view of the deaerator support platform. There are five #10 rebar rods in the tension zone of the support columns. The weight of the deaerator (including water) is assumed to be 0.4×10^6 pounds. A simplified method is used for the calculation of the base shear and base moment of the platform. In this method, the platform is treated as a one-degree-of-freedom system. The stiffness of the system, K, is determined by

$$K = 45 k ,$$

in which k is the stiffness of each individual column,

$$k = \frac{12EI}{\ell^3}$$

$$E = 3.6 \times 10^6 \text{ pounds per square inch}$$

(modulus of elasticity of concrete)

$$I = 5,460 \text{ inch}^4 \text{ (moment inertia of column)}$$

$$\ell = 384 \text{ inch (length of the column)}$$

thus,

$$k = \frac{12 \cdot (3.6 \times 10^6) \cdot 5460}{384^3}$$

$$= 4,150 \text{ pounds per inch}$$

and

$$K = 45 k$$

$$= 186,000 \text{ pounds per inch.}$$

The frequency of the platform is

$$\begin{aligned} f &= \frac{1}{2\pi} \left(\frac{K}{M} \right)^{1/2} \\ &= \frac{1}{2\pi} \left(\frac{186000 \times 386}{0.4 \times 10^6} \right)^{1/2} \\ &= 2.2 \text{ Hertz.} \end{aligned}$$

The spectral acceleration, S_a , can be determined from the standard design response spectrum commonly used in the U.S. (see figure 81) anchored at $ZPA = 0.3 \text{ g}$. The spectral acceleration obtained from figure 81 is 0.8 g , with a 5% damping ratio. Based on the equivalent static method, the base shear and base moment at the bottom of the column can be determined from -

$$\begin{aligned} \text{base shear force} &= W \cdot S_a \\ &= 0.4 \times 10^6 \times 0.8 \\ &= 0.32 \times 10^6 \text{ pounds.} \end{aligned}$$

$$\begin{aligned} \text{shear stress at} \\ \text{bottom of each column} &= \frac{0.32 \times 10^6}{246 \times 45} \\ &= 30 \text{ pounds per square inch} < 1000 \text{ pounds per square inch} \end{aligned}$$

(area of individual column = 246 square inches)

(allowable shear stress for M-400 concrete = 1000 pounds per square inch)

$$\begin{aligned} \text{base bending moment at each column} &= W \cdot S_a \cdot h / 45 \\ &= 0.4 \times 10^6 \times 0.8 \times 584 / 45 \\ &= 4.15 \times 10^6 \text{ inch-pounds} \end{aligned}$$

The base moment of 4.15×10^6 inch-pounds is used for the following detailed stress calculation.

Detailed Stress Calculation

Figure 82 shows the cross section, strain and stress diagrams. From figure 82,

$$\frac{\epsilon_c}{\epsilon_s} = \frac{x}{13.5 - x}$$

$$\frac{f_c}{f_s} = \frac{\epsilon_c \cdot E_c}{\epsilon_s \cdot E_s} = \frac{3 \times 10^6}{30 \times 10^6} = \frac{x}{13.5 - x} = \frac{x}{10(13.5 - x)}$$

The compressive and tension resultant forces, C and T, are equal to

$$\begin{aligned} C &= \frac{1}{2} f_c \cdot b \cdot x \\ &= \frac{1}{2} f_c \cdot 16 \cdot x \\ &= 8 f_c x \end{aligned}$$

$$T = f_s A_s = f_s \cdot 5 \cdot 1.27 = 6.35 f_s$$

Since $C = T$, therefore,

$$\frac{f_c}{f_s} = \frac{6.35}{8x}$$

and

$$\frac{6.35}{8x} = \frac{x}{10(13.5 - x)}$$

$$8x^2 + 6.35x - 857 = 0$$

$$x = 10 \text{ inch.}$$

$$C = T = \frac{M}{y} = \frac{4.15 \times 10^6}{13.5 - 3.3} = 0.415 \times 10^6 \text{ pounds}$$

$$f_c = \frac{C}{8x} = \frac{0.415 \times 10^6}{8 \times 10} = 5186 \text{ pounds per square inch} \quad \begin{array}{l} \text{(ultimate stress of} \\ \text{M-400 concrete)} \end{array}$$

$\approx 5000 \text{ pounds per square inch}$

$$f_s = \frac{0.415 \times 10^6}{6.35} = 65,000 \text{ pounds per square inch}$$

$< 75,000 \text{ pounds per square inch (yielding stress of rebar)}$

7.3 Seismic Capacity

$$\text{Elastic seismic capacity for concrete} = \frac{2800}{5186} \times 0.3 \text{ g} = 0.16 \text{ g} ,$$

$$\text{Elastic seismic capacity for rebar} = \frac{58}{65} \times 0.3 \text{ g} = 0.26 \text{ g} .$$

Note that the yield stress for concrete and rebar is 2,800 pounds per square inch and 58,000 pounds per square inch, respectively. The ultimate seismic capacity is

$$\text{ultimate seismic capacity} = 0.16 \times 2.5 = 0.4 \text{ g} .$$

Note that a ductility factor of 2.5 is used in the calculation of the ultimate seismic capacity.

8.0 Seismic Analysis of the VVER-440 Model V230 Reactor Support Including Annular Water Tank

8.1 Introduction

The VVER-440 model V230 reactor vessel is supported by an annular water tank which in turn is supported by the reinforced concrete reactor shaft. The major function of the annular water tank is for shielding. Figure 83 shows the details of the reactor support. The water tank is welded to twenty (20) support flanges around the circumferential direction of the tank. The flange has 6 inches (15 centimeter) thickness and 43 inches (108 centimeters) height as shown in figure 84. There are internal radial stiffeners inside the tank. The integrity of the support flange and the annular water tank is of vital importance to the reactor vessel. This section presents a seismic analysis on the integrity of the support flange and the annular tank. The major focus is on the vertical earthquake excitation. For simplicity, the internal stiffeners are not considered in the analysis.

8.2 Support Flange

The weight of VVER-440 model V230 reactor is about 542 tons. A total weight of 600 tons (including water) is used in the analysis

$$W = 600 \times 1000 \times 2.2 = 1.32 \times 10^6 \text{ pounds}.$$

The seismic loading in terms of peak vertical acceleration is 0.2 g (2/3 of peak horizontal acceleration). For conservative purposes, an amplification factor of five (5) is used. Therefore, the maximum downward seismic acceleration exerted by the reactor is 1 g. The total acceleration exerted by the reactor is 2 g (1 g due to the gravitation effect). The compressive stress of the support flange is

$$\sigma = \frac{2 \times 1.32 \times 10^6}{2.0 \times 11.75 \times 6} = 1800 \text{ pounds per square inch},$$

Note that an average length $[(7.8 + 15.7)/2 = 11.75 \text{ inches}]$ of the support flange is used in the calculation. The shear stress on the tank-flange interface is

$$s = \frac{2 \times 1.32 \times 10^6}{20 \times 43 \times 6} = 511 \text{ pounds per square inch}$$

Both the compressive and shear stresses are very small compared to steel yielding stresses (30,000 pounds per square inch) of the steel. Therefore, failure of support flange will not be expected during seismic events.

8.3 Annular Water Tank

Figure 85 shows an axisymmetric model for the annular tank. The internal ribs inside the tank are neglected in the analysis. The analysis indicates that the tank has a fundamental mode of 3.2 Hertz. The maximum seismic stress is 110,000 pounds per square inch. If the yield stress is assumed to be 40,000 pounds per square inch, the elastic seismic limit is

$$\text{elastic seismic capacity} = \frac{40}{110} \times 0.3 = 0.11 \text{ g} ,$$

and

$$\text{ultimate seismic capacity} = 0.11 \times 1.3 = 0.15 \text{ g} .$$

Since the internal ribs of the tank are not considered in the analysis, it is believed that the water tank should withstand seismic loads up to 0.2 g.

9.0 Sloshing Response in a VVER-440 Refueling Pool, Steam Generator, and Deaerator Tank

9.1 Introduction

Due to the presence of a large free surface, seismically induced sloshing can occur in many important areas in VVER plants such as the refueling pool, steam generator, deaerator tank, water storage tanks, etc. Fluid sloshing may be an important loading on the structure. The maximum wave height should be properly assessed. Water spillage may cause damage on the surrounding structures, components, and associated systems. Technical specifications issued after the 1977 Vrancea earthquake contain the following statement:

"The design of the tanks and vessels will preclude deterioration of system performance due to effects of internal liquid sloshing."

The sloshing motion is characterized by a low-frequency motion with standing waves on the free surface moving up and down. For example, a large liquid storage tank with dimensions of 20 meters x 15 meters (diameter x liquid depth) has a fundamental sloshing frequency of 0.25 Hertz (period = 4 seconds). For most water tanks, the fundamental sloshing frequency is usually below 1 Hertz.

This section examines the sloshing behavior in various liquid-contained systems of a VVER plant. Only selected items are examined. They include the VVER-440 model V230 refueling pool, steam generator, and deaerator tank. The main focus is on the sloshing wave height. Important issues related to sloshing are addressed.

9.2 Sloshing Wave Height in VVER-440 Model V230 Spent Fuel Storage Pool

The elevation view and top view of the reactor storage pool for VVER-440 model V230 spent fuel is shown in figures 86 and 87, respectively. The VVER-1000 has an individual storage pool in the auxiliary building. The pool is a concrete structure lined with a welded stainless steel panel (0.25 inches thick). The pool of the VVER-440 model V230 has a dimension of 6.75 meters x 3.15 meters x 7 meters (length x width x height). The spent fuel is removed from the core by the fuel handling machine through a narrow, water-filled canal

(0.5 meters wide) to the storage racks inside the pool. In a separate operation, the fuel is moved from the storage racks to the adjacent shipping casks for transport to away-from-reactor storage or fuel reprocessing facilities. A dam is provided in the canal for isolation of the storage pool from the reactor area.

Figure 88 shows a VVER-440 schematic view of fuel loading procedures. There are two water levels, as shown in figure 88. During the fuel transport process, the whole area including the top part of the reactor vessel is water filled for underwater movement of spent fuel. During normal operation, only the storage pool is filled with water, as shown in figure 88. Therefore, two separate models are constructed (see figure 89) for calculating the sloshing wave height. In the first model, the length of the tank is 6.75 meters, representing normal operation. In the second model, the length of the tank is 12 meters, representing the fuel transport condition. The water depth in both models is assumed to be 4.5 meters, which represents the effective depth of the water participating in the sloshing in both cases. The seismic loading is identical to that used in the seismic analysis of the VVER-440 main building, (i.e., 10 seconds, 0.3-g ground acceleration time history). The results of these two cases are presented herein.

Normal Operating Condition

The seismic loading has a duration of 10 seconds. The analysis, however, is carried out to 30 seconds. The maximum calculated wave height is 10.9 inches (27.7 centimeters), occurring at Node 91 which is next to the tank wall. The sloshing frequency is 0.33 Hertz. Figure 90 shows the calculated sloshing wave height at Node 91.

Fuel Transport Condition

The maximum wave height for the case of the fuel transport condition is 10.0 in (25.4 centimeters) occurring at Node 91. The sloshing frequency is 0.23 Hertz. Figure 91 shows the calculated wave height history at Node 91.

The sloshing pressures for both cases are very low. Future studies should consider the flexibility of the liner, because the fluid pressure caused by the liner vibration may be much higher than the sloshing pressure. The wave height, however, will not be affected by the flexibility of the liner. The results of this study are summarized below:

seismic loading = 0.3 g,

normal operating condition = 10.9 inches
maximum wave height

fuel transport condition = 10.0 inches.

In both cases, the maximum wave height is too low to cause any problems.

9.3 Sloshing in the Steam Generator

Figure 92 shows the steam generator in VVERs which is positioned horizontally with many submerged tube bundles. In a VVER-440 plant, the steam generator is hung from the ceiling, whereas the steam generator in a VVER-1000 plant is bottom supported. The dimensions of the steam generator are about 3.2 meters x 11.5 meters (inside diameter x length) for the VVER-440 and 4.0 meters x 13.8 meters for the VVER-1000. Since the steam generator is horizontal, there exists a large, free-surface. A strong sloshing motion will occur during

seismic events. The sloshing motion is further pronounced in the VVER-440 plant. Since the steam generator is hung from the ceiling, the low-frequency vibration of the primary loop (i.e., 0.3 Hertz for the VVER-440 primary loop) will also enhance the sloshing motion which is also characterized by the low-frequency vibration. This situation should be analyzed in future studies.

9.4 Sloshing in the Deaerator Water Storage Tank

The deaerator of a VVER plant usually consists of two deaerating columns and a large horizontal cylindrical tank underneath the columns for collecting the water. Figure 93 shows the deaerator tank configuration. The tank has dimensions of 13 m x 3.5 m (length and diameter). The water in the deaerating tank may be rather high, but it should not exceed the height of the tank so as not to fill the deaerating column, thus disturbing its operation. For this reason, the deaerating tank is always designed with an overflow device at the elevation of the maximum permissible water level. With the overflow device, it appears that the seismic sloshing should not cause any serious problems for the deaerator tank.

10.0 Storage Tank Integrity Assessment under Seismic Loading

10.1 Background

The seismic design of the vertical storage tank has been largely based on Housner's theory which assumes the tank behaves as a rigid body. Thus, the tank wall experiences the same acceleration as the input base acceleration. The rigid wall assumption enables Housner's theory to provide a simple and fast procedure for determining the seismically induced hydrodynamic forces exerted on the tank walls. Although some doubt had been expressed from time to time about the validity of this assumption for thin-shell tanks, experimental data and experience in strong-motion earthquakes were not available to either verify the appropriateness of this approach or to identify its weaknesses.

The subject appeared to be reasonably settled and the overall validity of the simplified approach accepted in design practice until the 1964 Alaska earthquake. During that earthquake, large-scale damage occurred to petrochemical tanks. Of further significance, damage to water storage tanks such as buckling of tank walls and liftoff of the anchored tanks from foundations occurred during the 1971 San Fernando earthquake in California. Most of these failures are suspected to be the consequence of dynamic buckling caused by excessive overturning moments of seismically induced liquid inertia and surface sloshing waves. Those failures led to greatly increased attention to the analysis of the dynamic characteristics of the liquid tank systems. In particular, considerable experimental and analytical studies of the dynamic behavior of fluid-tank systems have been conducted since the 1971 San Fernando earthquake.

10.2 Storage Tanks in VVER Plants

The VVER plants contain various tanks and reservoirs, the most important among them being storage tanks for feedwater and condensate, drain water collecting tanks, tanks for radioactive (and nonradioactive) liquids, etc. According to a recent PRA study on US plants, large storage tanks such as refueling water storage tanks are critical elements contributing to the seismic PRA. For some old plants, the anchor supports are weak links in assessing tank seismic integrity. Therefore, the seismic integrity of the large storage tanks in VVER plants should be a design consideration.

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2. N. M. Newmark, "A Response Spectrum Approach for Inelastic Seismic Design of Nuclear Reactor Facilities," Proceedings of the 3rd International Conference on Structural Mechanics in Reactor Technology, London, England, paper K5/1, Vol. 4, 1975.
3. N. M. Newmark and W. J. Hall, "Development of Criteria for Seismic Review of Selected Nuclear Power Plants," NUREG/CR-0098.

Table 1 Beam Properties

Beams	Area A (square inches)	Moment of Inertia I (square inches x square inches)
1-3 (between two mats)	1.44×10^6	9.2×10^{11}
4-7 (reactor section)	5.76×10^5	1.45×10^{11}
8-15 (cylindrical shell)	2.6×10^5	9.5×10^{10}

Table 2 Soil Properties and Foundation Impedance

Item	Soft Site	Medium Site	Hard Site
Shear Wave	2,000	4,000	6,000
Velocity, V_s, feet per second			
Unit wt. (thousand pounds per cubic foot)	.155	.155	.155
Shear Modulus G (thousand pounds per cubic foot)	19,000	77,500	170,000
K_x (thousand pounds per foot)	1.6×10^7	6.4×10^7	15×10^7
C_x (thousand pounds • second per foot)	1×10^6	1.96×10^6	3×10^6
K_d (thousand pounds • feet per radian)	0.3×10^{12}	1.23×10^{12}	2.7×10^{12}
C_d (thousand pounds • feet • second)	7×10^9	14×10^9	22×10^9

Table 3 Maximum Shear Force and Bending Moment

Beam Element		Shear (x 10 ⁶ pounds)	Moment (x 10 ⁹ inch-pounds)
1		135	175
2	(below 13 m level)	125	145
3		115	95
4		50	28
5	(reactor section)	40	16
6		115	6.5
7		10	1.4
8		50	70
9		47	60
10	(cylindrical	44	50
11	containment	40	34
12	shell)	35	24
13		30	14
14		23	7.5
15		15	2.0

Table 4 Shear Stresses and Bending Stresses at Critical Locations

Beam Element Number	Shear Stress (pounds per square inch)	Bending Stress (pounds per square inch)
Element 1 (bottom of building)	190	250
Element 4 (bottom of reactor section)	175	170
Element 8 (bottom of cylindrical containment)	384	654 (> 500 pounds per square inch)

tensile strength of M-400 concrete = 500 pounds per square inch

shear strength of M-400 concrete = 1,000 pounds per square inch

Cross-sectional view of the main building

1—reactor; 2—steam generator; 3—main circulating pump; 4—main circulation gate valve; 5—turbogenerator; 6—deacerator; 7—refueling pond; 8—plenum ventilation centre; 9—pipe aisle; 10—control board room; 11—cable shelf; 12—switchgear location; 13—exhaust ventilation centre

Plan of the main building

1—reactor; 2—steam-generator cell; 3—modular control board room; 4—exhaust ventilation centre; 5—transport aisle; 6—switchgear location; 7—turbogenerator

Figure 1 Cross-Sectional View and Plan View of the VVER-440 V230 Main Building

Figure 2 Skelton of the VVER-440 Model V230 Main Building

Figure 3 Dimensions of the Cross Section and Rebar Distribution of the Columns

Ribbed roof slab 3×12 m in plan

Figure 4 Roof-Slab Configuration of the VVER-440 Model V230 Main Building

Figure 5 Mathematical Model of the VVER-440 Model V230 Main Building

Figure 6 Lumped Weight (pounds) at Various Locations

Figure 7 0.3 g Ground Acceleration Used in the Seismic Analyses

TIME = 1.000

Figure 8 Deformed Configuration at $t = 1$ second (amplification factor = 500)

TIME = 8.000

**Figure 9 Deformed Configuration at $t = 8$ second
(amplification factor = 500)**

Figure 10 Calculated Acceleration Response at Node 9

Figure 11 Calculated Acceleration Response at Node 11

Figure 12 Calculated Acceleration Response at Node 37

Figure 13 Calculated Acceleration Response at Node 39

Figure 14 Calculated Acceleration Response at Node 53

Figure 15 Calculated Maximum Acceleration (g) at Various Locations

Figure 16 Calculated Maximum Displacement (inches) at Various Locations
Figure 19 Cross-Sectional View of the Column with
Dimension of 1.5 meters x 0.6 meters

**Figure 17 Calculated Maximum Bending Moment ($\times 10^6$ inch-pounds)
at Various Members**

Figure 18 Calculated Maximum Axial Force ($\times 10^6$ pounds) in the Girders and Roof Truss

(a) CROSS SECTION (b) STRAIN (c) STRESS

Figure 19 Cross-Sectional View of the Column with
Dimension of 1.5 meters x 0.6 meters

Figure 20 Cross-Sectional View of the Column with
Dimension of 1.2 meters x 0.6 meters

Figure 21 Cross-Sectional View of the Column with
Dimension of 0.8 meters x 0.6 meters

Figure 22 Cross-Sectional View of the Column with
Dimension of 0.75 meters x 0.6 meters

Figure 23 Cross-Sectional and Plan View of the VVER-1000 Main Building

Figure 24 Plan Views at Various Elevations

Figure 25 Mathematical Model of the VVER-1000 Main Building

Figure 26 Floor Thickness at Various Elevations of the VVER-1000 Main Building

Figure 27 Lumped Weight at Various Locations

Figure 28 0.3 g Ground Input Acceleration

TIME.= 3.000

Figure 29 Enlarged Deformation Configuration

Figure 30 Acceleration Fourier Spectrum at Top of Containment

Figure 31 Acceleration Fourier Spectrum at Top of Reactor Section

Figure 32 Displacement at Base of Reactor Building

Figure 33 Displacement at Top of Reactor Section

Figure 34 Displacement at Top of Containment

VVER1000 STICK MODEL - NODE 07

TMAX,AMAX TMIN,AMIN= 0.71 0.0934 3.72 -0.0742

VVER1000, NODE 07 - 13 METER LEVEL

HORIZONTAL RESPONSE SPECTRA WITH 3,5,7% DAMPING

Figure 35 Calculated Relative Displacement and Floor Response Spectra at Node 7

VVER1000 STICK MODEL - NODE 15

TMAX,AMAX TMIN,AMIN= 0.71 0.2094 0.52 -0.1733

HORIZONTAL RESPONSE SPECTRA WITH 3,5,7% DAMPING

Figure 36 Calculated Relative Displacement and Floor Response Spectra at Node 15

VVER1000 STICK MODEL - NODE 31

TMAX,AMAX TMIN,AMIN= 0.72 0.8610 0.58 -0.7870

HORIZONTAL RESPONSE SPECTRA WITH 3,5,7% DAMPING

Figure 37 Calculated Relative Displacement and Floor Response Spectra at Node 31

Figure 38 Calculated Maximum Acceleration at Various Locations

**Figure 39 Calculated Bending Moment History of Element 1
(bottom of the building)**

**Figure 40 Calculated Shear Force of Element 1
(bottom of the building)**

**Figure 41 Calculated Bending Moment of Element 4
(bottom of reactor section)**

**Figure 42 Calculated Shear Force of Element 4
(bottom of reactor section)**

**Figure 43 Calculated Bending Moment History to Element 8
(bottom of cylindrical containment)**

**Figure 44 Calculated Shear Force of Element 8
(bottom of cylindrical containment)**

$$\begin{aligned}
 f_8 &= 47 \times 10^6 \times (1.4 + 1.1 + 0.83 + 0.57)/4 \\
 &= 47 \times 10^6 \times 0.96 \\
 &= 45 \times 10^6 \# \\
 &\quad (\text{FLUSTR-ANL: } 55 \times 10^6 \#)
 \end{aligned}$$

$$\begin{aligned}
 f_4 &= 79 \times 10^6 \times (.77 + .70 + .60 + .48) \\
 &= 79 \times 10^6 \times .63 \\
 &= 50 \times 10^6 \# \\
 &\quad (\text{FLUSTR-ANL: } 55 \times 10^6 \#)
 \end{aligned}$$

$$\begin{aligned}
 f_1 &= f_1 + f_2 + 150 \times 10^6 \times (.39 + .36 + .32)/3 \\
 &= 95 \times 10^6 + 150 \times 10^6 \times .39 \\
 &= 150 \times 10^6 \# \\
 &\quad (\text{FLUSTR-ANL: } 140 \times 10^6 \#)
 \end{aligned}$$

Figure 45 Hand Calculations of the Reactor Forces

Figure 46 Bending Moment and Shear Forces at Critical Areas

$$\text{Pitch} = \frac{300 \text{ un}}{2.54 \times 22} = 5.3 \text{ in}$$

Use 5" in the analysis

Total number of rebars in 180° (two layers of mesh)

$$= \frac{3.14 \times 900 \times 2}{5} = 1130$$

Figure 47 Total Number of Rebars

Figure 49 Rebar Arrangement in the Containment

Figure 50 Simplified Cross Section of the Containment

Figure 51 Prestressed Concrete Containment of VVER-1000

Figure 52 Top View of Primary Loop of VVER-440 Model V230

Figure 53 Mathematical Model of VVER-440 Model V230 Primary Loop

Figure 54 Locations of the Springs Simulating the Hydraulic Shock Absorbers

BEAM STICK MODEL - VVER-441A - NODE 41

TMAX,AMAX TMIN,AMIN= 4.81 14.5009 3.56 -13.1822

BEAM STICK MODEL - VVER-441A - NODE 41

MAX. FREQUENCY,AMPLITUDE= 0.30 4.0429

Figure 55 Calculated Movement at Steam Generator (identical to the Measured Value During 1977 Vrancea Earthquake Under 0.1 g Loading)

Figure 56 Maximum Lateral Movement at Various Locations

BEAM STICK MODEL - VVER-441A - NODE 41

TMAX,AMAX TMIN,AMIN= 3.70 171.6430 0.39 -115.9750

BEAM STICK MODEL - VVER-441A - NODE 41

MAX. FREQUENCY,AMPLITUDE:= 0.30 15.9806

Figure 57 Calculated Acceleration Time History and
Fourier Spectrum at the Steam Generator

Figure 58 Calculated Maximum Relative Acceleration, g,
(with respect to reactor nozzle) at Various Locations

Figure 59 Top View of the Primary Loop Reinforced by the Shock Absorbers

Figure 60 Maximum Displacement at Various Locations of the Primary Loop Reinforced by Shock Absorbers

Figure 61 Deformed Configuration (amplified 1000 times)

BEAM STICK MODEL - VVER-440B - NODE 75

TMAX,AMAX TMIN,AMIN= 1.94 0.0749 0.39 -0.0964

BEAM STICK MODEL - VVER-440B - NODE 75

MAX. FREQUENCY,AMPLITUDE= 11.30 0.0036

Figure 62 Calculated Displacement and Fourier Spectrum at Node 75

Figure 64 Elevation View of Primary Loop of VVER-1000

Figure 65 VVER-1000 Steam Generator Supports

Figure 66 Mathematical Model of the Primary Loop of the VVER-1000

Figure 67 0.603 g Acceleration Input Motion Used in the Analysis

Figure 68 Calculated Displacement Time History at the Steam Generator

VVER-1000 PIPING Y-DISP. NODE-65(HOT LEG)

TMAX,AMAX TMIN,AMIN= 3.87 6.2689 4.42 -7.3576

VVER-1000 PIPING Y-DISP. NODE-65(HOT LEG)

MAX. FREQUENCY,AMPLITUDE= 0.90 1.6203

Figure 69 Calculated Displacement Time History at Primary Loop

**Figure 70 Calculated Maximum Displacements at Various Locations
(with no shock absorbers)**

Figure 71 Location of the Springs Simulating the Shock Absorbers

**Figure 72 Calculated Maximum Displacements
(loop reinforced by shock absorbers)**

Figure 73 Maximum Displacements at Various Locations
(capacity of shock absorber is doubled)

Figure 74 Accident Localization Compartment of the VVER-440 Model V230 (elevation view)

Figure 75 Isometric View of the VVER-440 Model V230 Accident Localization Compartments

Figure 76 Mathematical Model of the Accident Localization Compartment

CSMP RUN 23 - SAFR - NODE 18

TMAX,AMAX TMIN,AMIN= 7.32 153.8860 5.58 -124.0910

CSMP RUN 23 - SAFR - NODE 18

MAX. FREQUENCY,AMPLITUDE= 4.15 3.6817

Figure 77 Vertical Ground Motion and Fourier Spectrum

TIME = 1.000

Figure 78 Deformed Configuration

**Figure 79 Schematic Diagram of the Deaerating Column
with a Tank**

Figure 80 Location of the Deaeration Tank

Figure 81 Response Spectrum of the Ground Motion

Figure 82 Cross Section of the Columns

Figure 83 Annular Water Tank of VVER-440 Model V230

Figure 84 Support Flange of the Annular Water Tank

**Figure 85 Mathematical Model and Deformed Configuration
of the Annular Water Tank**

Figure 86 Location of the VVER-440 Model V230 Refueling Pond

Figure 87 Top View of the Refueling Pool

1 Reactor	8 Storage Pool
2 Re-Fueling Machine	I - Reloading of Spent Fuel from Reactor to Storage Pool
3 Racks for Spent Fuel	II - Reloading of "Cooled" Spent Fuel into Transport Container
4 Receiving Container	9 Level of Water During Refueling
5 Railroad Transport	10 Spent Fuel Element
6 Transport Container	11 Level of Water During Storage
7 Reactor Hall Bridge Crane	

VVER-440 Schematic of Fuel Loading Procedures

Figure 88 Water Levels During Normal Operation and Refueling

Level of Water During Storage

Refueling

Figure 89 2-D Mathematical Model of the Refueling Pool of Normal Operation (top figure) and Refueling Operation (bottom figure)

**Figure 90 Calculated Sloshing Wave Height at Node 91
(normal operation)**

Figure 91 Calculated Sloshing Wave Height at Node 91
(refueling condition)

Figure 92 Configuration of Steam Generator

Figure 93 Sloshing in the Deaerating Storage Tank

ANALYSIS APPENDIX G
EXAMINATION OF S-SEAL INTEGRITY

TABLE OF CONTENTS

	Page
1.0 Introduction	G-1
2.0 Design Description	G-1
3.0 Three-Dimensional Structural Model	G-2
4.0 Results	G-2
5.0 Summary	G-3
References	G-3

FIGURES

1. Configuration of Reactor Shaft with S-Seal
2. Design Details of S-Seal
3. Perspective View of Finite Element Model
4. Elevation End View of Finite Element Model
5. Top View of Finite Element Model
6. Perspective View of Deformed S-Seal
7. Elevation End View of Deformed S-Seal

ANALYSIS APPENDIX G

EXAMINATION OF S-SEAL INTEGRITY

1.0 Introduction

Pressures developed during postulated and hypothetical accidents may challenge the structural integrity of the accident localization system (ALS) pressure boundary of a VVER-440 model 230. One of the components in the accident localization system is an S-seal, which is a bellows-type pressure seal located between the reactor pressure vessel and the concrete annulus. This seal is internal to the ALS pressure boundary. The S-seal provides a boundary between the upper and lower shaft regions. The capability of this boundary is of interest. However, an upper shaft overpressure of 0.1 megapascal (14.5 pounds per square inch) will rupture a membrane between the upper shaft and the steam generator room to provide for blowdown. This appendix examines the structural response of the S-seal to overpressures that might result from accident scenarios (Analysis Appendix C).

The NEPTUNE¹ finite element computer program is used to evaluate the structural response of the S-seal (VVER-440 model V230) to the overpressure that might result from postulated accidents. A complex three-dimensional model was developed to represent a 7.5 degree circumferential sector of the seal. Boundary conditions were imposed to simulate the reinforcing ribs and flange connections. The seal's material properties were taken to elastoplastic.

Both static and dynamic pressure loading were considered. The integrity of the S-seal at the accident pressure levels identified in Analysis Appendix C was determined.

2.0 Design Description

Figure 1 shows the configuration of the reactor shaft with the S-seal. The reactor pressure vessel is in the reactor shaft, a cylindrical shaped cavity. The S-seal is located at the concrete support flange, which is slightly above the inlet and outlet piping. Figure 2 provides details of the seal and its attachment to the vessel and concrete flange. The bellows is constructed from three thin annular plates (lower, middle, and upper) that are welded to two semi-circular toroids. The plates are assumed to be 4 millimeters thick. The upper annular plate is welded at its inner diameter to the bellows upper flange. The upper flange is welded along the upper part of its inner diameter to the reactor vessel. A stiffener rib is welded to the bottom of the upper support flange to support the upper annular plate when the upper shaft volume is flooded for refueling. The lower annular plate of the bellows is welded to the lower support flange that is embedded into the annular concrete bracket. A stiffener rib is welded to the lower support flange. The upper and lower stiffeners are taken to be spaced at 15 degree intervals in the circumferential direction.

The S-seal is fabricated from carbon steel that was assumed to have properties similar to the Soviet carbon steel designated as 22 K.² This steel has a Young's modulus of 207 gigapascal (30×10^6 pounds per square inch) and a Poisson's ratio of 0.3. The yield and ultimate stresses are 265 megapascal (38×10^3 pounds per square inch) and 431 megapascal (62×10^3 pounds per square inch), respectively. The ultimate strain is 22 percent.

3.0 Three-Dimensional Structural Model

A complex finite element model (figures 3 through 5) for the S-seal was developed. The model only had to include a 7.5 degree circumferential sector because of the symmetry that exists in the bellows between stiffeners. Thus, in the

circumferential direction, one end of the model is at the stiffener rib and the other end is midway between adjacent stiffeners. The model represents the upper, middle, and lower annular plates and the upper and lower semi-circular toroid segments. A total of 405 Mindlin type quadrilateral plate elements and 448 nodes are used in the discretization.

The upper and lower flange and the upper and lower stiffeners are not modeled explicitly. Since these structures are substantially thicker than the S-seal, it was felt that this analysis could ignore them because they would probably not fail before the S-seal. However, the constraining effect of the flanges and stiffeners on the S-seal is taken into account with boundary conditions.

A completely fixed boundary condition (i.e., no translations or rotations permitted) is imposed at the inner diameter of the upper annular plate and at the outer diameter of the lower annular plate. This condition should simulate the rigid constraint imposed by the flanges. There are vertical symmetry planes located at each end of the model. The first plane contains the stiffener ribs and has the following boundary conditions imposed on the nodes within it. Translational motion is restricted to the symmetry plane, and rotational motion out of the plane is prevented. The nodes that are adjacent to the bottom of the lower stiffener and those between the upper and lower stiffeners are constrained to move in the horizontal direction. The nodes that lie in the other vertical symmetry plane are restricted to translational and rotational motion within that plane. All the remaining nodes in the model are unconstrained.

A pressure load was applied normal to the inside surface, which is the side that would be subjected to pressure within the lower shaft volume. The model was subjected to both static and dynamic pressure histories.

4.0 Results

This section describes the response of the bellows model to static and dynamic overpressures. The first analysis looked at the response of the S-seal to increasing static pressures, and the second analysis looked at the response due to a ramp pressure-time history.

For the static analysis, the inner surface of the finite element model was pressurized incrementally. At a pressure level of 0.7 megapascal absolute (102 pounds per square inch absolute), the material at the inner diameter of the upper annular plate begins to undergo plastic deformation. Since the failure pressure for the VVER-440 model V230 localization compartment walls was estimated to be about 0.4 megapascal absolute (58 pounds per square inch absolute) (Analysis Appendix E), the static loading calculation was not continued beyond 0.7 megapascal absolute (102 pounds per square inch absolute).

The S-seal was also analyzed for a ramp pressure pulse for the purpose of gaining insight about the shape that the S-seal could possibly take under large deformations. This would allow us to understand the potential for failure and failure modes of the seal. The following hypothetical pressure pulse was applied: at 0.0 milliseconds the pressure was 0.0 megapascal absolute (0.0 pounds per square inch absolute); the pressure linearly increased to 2.1 megapascal absolute (300 pounds per square inch absolute) at 0.2 milliseconds; after 0.2 milliseconds the pressure remained constant at 2.1 megapascal absolute (300 pounds per square inch absolute).

The deformed shape is shown in figures 6 and 7. It is seen that the upper annular plate and the upper semi-circular toroid experience large deformation over the entire circumferential sector. In contrast, the middle and lower annular plates and the lower toroid are somewhat restricted in their motion by the upper and lower stiffener ribs. These constraints cause the middle and lower plates to deform into the shape of a trough. Material yielding is occurring throughout the entire structure. The largest distress is occurring in the middle annular plate and in the lower toroid near the stiffener rib. High stresses also occurred at the radial boundaries of the upper and lower annular plates.

5.0 Summary

A nonlinear finite element analysis of a VVER-440 model V230 S-seal has been undertaken to understand its response to overpressure. The calculated pressure to fail concrete walls of the ALS was 0.4 megapascal absolute (58 pounds per square inch absolute) (Analysis Appendix E). A static analysis of the model indicated that the S-seal would respond elastically up to about 0.7 megapascal absolute (102 pounds per square inch absolute), which is well above the concrete wall failure pressure. A dynamic analysis was performed to inexpensively approximate the shape of the S-seal at large deformations. This analysis indicated that high stresses occur in the S-seal in the regions near the stiffener ribs and at the locations where the S-seal attaches to the upper and lower support flanges.

References

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Figure 1 Configuration of Reactor Shaft with S-Seal (bellows seal)

Figure 2 Design Details of S-Seal

Figure 3 Perspective View of Finite Element Model

Figure 4 Elevation End View of Finite Element Model

Figure 5 Top View of Finite Element Model

Figure 6 Perspective View of Deformed S-Seal

Figure 7 Elevation End View of Deformed S-Seal

ANALYSIS APPENDIX H
REACTOR TRANSIENT ANALYSES

Table of Contents

1.0 Introduction	1
2.0 Modeling	1
2.1 The RETRAN Computer Code	1
2.2 Simplified Point Kinetics Model	2
2.3 VVER-440 Model V230	2
2.4 VVER-440 Model V213	2
2.5 VVER-1000 Model V320	3
3.0 VVER Reactivity Transients Analyses	3
3.1 Reactivity Transient Analyses using RETRAN	3
3.1.1 50 cent Step Insertion	3
3.1.2 2 cent/second Ramp Insertion	3
3.1.3 Overcooling Transient Without Scram	4
3.1.4 Comparison of Critical Heat Flux Correlations	4
3.2 Reactivity Transient Analyses Using the Simplified Point Kinetics Model	4
3.2.1 Overcooling of the Core	4
3.2.2 Loss of Flow	5
3.2.3 Rod Ejection Accident	5
3.2.4 Six Rod Run-away Accidents	5
3.2.5 Excess Boron Removal Accidents	5
3.3 VVER-1000 Transients	5

TABLES

1. **Key Parameters for the VVER-440 and the VVER-1000s**
2. **Failure Modes and Effects (FMEAs) of VVER I&C Systems**
3. **Summary of Results of RETRAN Reactivity Transient Analysis**
4. **Transients Analysed Using the Simplified Point Kinetics Model**

FIGURES

- 1 **42 C Drop In Inlet Temp Without Scram - VVER-440**
- 2 **Flow Reduction to 50% - VVER-440**
- 3 **Rod Ejection Without Scram at 100% Power - VVER-440**
- 4 **Rod Ejection Without Scram at 1% Power - VVER-440**
- 5 **2 Rod Ejection Without Scram at 100% Power - VVER-440**
- 6 **2 Rod Ejection Without Scram at 1% Power - VVER-440**
- 7 **6 Rod Runaway Without Scram at 100% Power - VVER-440**
- 8 **6 Rod Runaway Without Scram at 1% Power - VVER-440**
- 9 **Boron Dilution at 1% Power - VVER-440**
- 10 **10 C Drop In Inlet Temp Without Scram - VVER-1000**
- 11 **Flow Reduction to 50% - VVER-1000**
- 12 **Rod Ejection Without Scram at 100% Power - VVER-1000**
- 13 **Rod Ejection Without Scram at 1% Power - VVER-1000**
- 14 **2 Rod Ejection Without Scram at 100% Power - VVER-1000**
- 15 **2 Rod Ejection Without Scram at 1% Power - VVER-1000**
- 16 **6 Rod Runaway Without Scram at 100% Power - VVER-1000**
- 17 **6 Rod Runaway at 1% Power - VVER-1000**
- 18 **Boron Dilution at 1% Power - VVER-1000**
- 19 **Allowable Low Limit Position of Regulating Rods**

ANALYSIS APPENDIX H

REACTOR TRANSIENT ANALYSES

1.0 Introduction

This appendix describes the results of analyses of the response of VVER power plants during various postulated and hypothetical transients. Both simplified screening calculations and more detailed methods were used. Particular emphasis was placed on reactivity events including simulated rod withdrawal accidents, simulated rod ejection accidents, and anticipated transients without scram (ATWS).

2.0 Modeling

Modeling was done using two different approaches, a point kinetics code/developed for the purpose) for screening calculations and to determine a number of events and more detailed analyses using RETRAN-2 MODE). The steady-state thermal hydraulic parameters were calculated using the RETRAN-02 Mod2 computer code. Some initial transients were analyzed using RETRAN. Due to the large number of transients that were identified as being of interest, and problems that would have been associated with running all of these transients using RETRAN, simplified point kinetics models were prepared for the purpose of analyzing a number of VVER-440 and VVER-1000 reactivity transients. Some results of these transients are presented in this appendix and discussed below. In addition three transients were run for both the VVER-440 and the VVER-1000.

Computer input models for the VVER-440 models V230, models V213, and the VVER-1000 model V320 were constructed and tested. At the time these accidents were simulated, the CAD/CAM views of the VVER-440 models V230 and the VVER-1000 models V320 were not available. System volumes and elevations were estimated from diagrams shown in DOE-NE-0084. System and plant component performance data were also taken from this document. To the extent possible, the models were constructed on a best estimate basis.

The reactivity coefficients for the VVER-440s and the VVER-1000s are different. Therefore, two different models were developed. Table 1 is a comparison of the parameters for the two reactor types.

2.1 The RETRAN Computer Code

The Electric Power Research Institute (EPRI) developed code, RETRAN, has been used extensively since 1978 and is considered to be generally applicable to large and small break loss of coolant accidents, one dimensional pressurized water reactor transients, and anticipated transients without scram. Features of the RETRAN code used to model VVER reactors are discussed here. Full details on the RETRAN code are contained in "RETRAN-02-- A Program for Transient Thermal-Hydraulic Analysis of Complex Fluid Flow Systems", EPRI NP-1850-CCMA, 1984.

RETRAN uses a one-dimensional, homogeneous equilibrium mixture thermal-hydraulic model for the reactor cooling system. The pressurizer (volume compensator) was modeled using the nonequilibrium option. The

heat transfer library included two-phase natural convection heat transfer correlations. Point kinetics was used to model the reactor neutronics for these preliminary analyses (a one-dimensional neutronic model is available). The reactor shutdown system was assumed to function successfully with a 1 second delay during the transient. The decay heat curves were consistent with those in Background Appendix F, "Decay Heat Generation in VVERs".

The model was limited to the main components of the primary coolant system. The pressurizer was modeled using a two region non equilibrium model. Although the output of the sprays and the capacity of the pressurizer heaters were known, the control system logic for them was not known, therefore these were not modeled. The core region was modeled as a single channel, single pin. The pump models used are discussed in the individual model sections below. The isolation valves present in the VVER-440 models V230 and model V213 were modeled, but these were not assumed to close during the transients. The horizontal steam generators were modeled as heat exchangers with the secondary side temperature and flow rate specified.

2.2 Simplified Point Kinetics Model

The RETRAN code is formulated to operate in the near-full power region and cannot be made to stabilize at initial conditions for power levels below approximately 10 percent full power. Transients of reactivity beginning when the reactor is operating far below full power are often more challenging to the plant protection system than are those beginning near full power. Therefore, a more simplified point kinetics model that could be run on a tabletop computer was prepared that including only the fuel and moderator coefficients to do screening analyses to study events initiated at very low power. It considered only reactivity effects brought about by conditions in the immediate region of the core and included nothing beyond the primary circuit. The thermal-hydraulic parameters came from the steady-state RETRAN model. The reactivity coefficients were also taken from Table 1.

The objectives of the analyses performed on the simplified point kinetics model were to predict the hotspot of the fuel centerline temperature and the relative neutron flux for accidents deemed to be capable of exceeding reasonable limits for these variables.

2.3 VVER-440 Model V230

VVER-440 model V230s have two significant features that distinguish them from VVER-440 model V213s; 1) canned rotor, low inertia primary pumps 2) makeup pumps of limited capacity (100 cubic meters per hour [433 gallons per minute]) instead of an emergency core cooling system. The pumps were modeled using the standard RETRAN pump curves, but with extremely low inertias. The coastdown time was determined by tripping the pumps with no break. The coastdown time of 3 seconds was found to be in agreement with values stated in the literature.

2.4 VVER-440 Model V213

The VVER-440 model V213 model was identical to that used for the VVER-440 model V230 except for the pump model and emergency core cooling system. The standard centrifugal pump model in RETRAN was

used. A series of pump trips was run using the values of the pump characteristics given in the literature until reasonable coastdown times were obtained.

2.5 VVER-1000 Model V320

The piping lengths for the VVER-1000 model V320 were estimated from the known mass of the primary piping system. Elevations were obtained from diagrams in DOE/NE-0084. The capacities of the components of the emergency core cooling system, the core flow areas, heat transfer area, volume, etc., were from Soviet literature describing a VVER-1000 model V320.

3.0 VVER Reactivity Transients Analyses

A failure modes and effects analysis (FMEA) was done to help determine the cases for which more extensive analyses were needed to resolve questions. Table 2 provides a summary of results. Some of the rationale for the events included in the FMEA are discussed in this section. Table 3 lists the transients analyzed.

3.1 Reactivity Transient Analyses using RETRAN

Three reactivity transient analyses were done for the VVER-440 and the VVER-1000 using the data given in Table 1. The transients were started at 100% power. With the exception of the overcooling event, the scram system was set to trip on an overpower condition with an 0.5 second delay time (1.10 for the VVER-440 model V230 and 1.07 for the VVER-1000 model V320). The results of these analyses are summarized in Table 3. As these analyses were done with a point kinetics model, local effects could not be identified. However, the feedback coefficients were weighted on a local basis axially for each core channel.

Departure from nucleate boiling ratios were calculated using the auxiliary hot channel model available in RETRAN. A Soviet developed correlation was used to calculate the critical heat flux. This is discussed further in 3.1.4.

3.1.1 50 cent Step Insertion

A 50 cent step insertion was run for a VVER-440 model V230 and a VVER-1000 model V320. The 50 cent step is typical of the reactivity addition that occurs in a single control rod ejection accident. The results, summarized in Table 3, show that the event is relatively mild, with the main thermal hydraulic parameters changed slightly from steady state conditions.

3.1.2 2 cent/second Ramp Insertion

A 2 cent/second ramp insertion was simulated for both the VVER-400 model V230 and the VVER-1000 model V320. This reactivity rate is approximately the normal rate for a single control rod withdrawal. A total of 50 cents was inserted. It should be noted that normally the control rods are withdrawn in banks. This transient was also relatively mild, although the pressures obtained were somewhat greater than in the step insertion case. It should be noted that for the VVER-440 model V230 that the power level reached is insufficient to cause scram and that the reactor is operating at a new higher steady state power level. The long term effects of this power to flow mismatch were not examined.

3.1.3 Overcooling Transient Without Scram

Because the reactivity feedbacks are negative, an overcooling event was simulated by increasing the heat removal rate from a single steam generator to the full nominal power of the reactor. This reduced the inlet temperature to the reactor by about 100 degrees Centigrade. This results in a positive insertion of reactivity. Although point kinetics was used, the reactivity effects were accounted for on a local basis. However, no attempt was made to account for skewing in the flux shape that might occur in the vicinity of a stuck rod. The MDNBR in both cases was calculated to be less than 1, with the possibility of fuel clad damage.

3.1.4 Comparison of Critical Heat Flux Correlations

The critical heat flux correlation developed by the Soviets was used to calculate the minimum departure from nucleate boiling ratios reported in this appendix and in Analysis Appendix D, "Fuel/Core Temperature Analyses". This correlation was developed from an extensive experimental data base compiled from a facility using an electrically heated, seven pin, full length test assembly. A total of 705 experimental points were then least squares fitted to the following empirical correlation:

$$q = \frac{a_2 + a_3 p}{a_1(1-x)} \frac{a_4 + a_5(1-x)}{G} \frac{1}{(1 + a_6 p)}$$

where,	q	=	critical heat flux (megawatts/square meter)
	x	=	quality
	G	=	mass flux (kilograms/square meter second)
	p	=	pressure (Megapascals)
	a ₁	=	0.795
	a ₂	=	-0.5
	a ₃	=	0.105
	a ₄	=	-0.127
	a ₅	=	0.311
	a ₆	=	-0.0185

A comparison was made between this correlation and the Babcock and Wilcox-2 correlation used in RETRAN. The critical heat fluxes from the correlations were plotted versus the quality for specified values of the pressure, mass flow rate, and thermal hydraulic diameter. The critical heat fluxes for the lower qualities typical of the normal range in a approximately 20% lower using the Soviet correlation than using the Babcock and Wilcox-2 correlation.

The Soviet correlation is used by the Soviets for both the VVER-440 and the VVER-1000. This is interesting since the VVER-440 uses a ducted assembly which permits no cross-flow while the VVER-1000 uses an open lattice assembly. Typically, cross-flow accounts for anywhere from 20% to 100% of the margin to DNB.

3.2 Reactivity Transient Analyses Using the Simplified Point Kinetics Model

3.2.1 Overcooling of the Core

VVER-440s have a relatively large negative temperature coefficient for both the fuel and moderator. These negative coefficients act to stabilize the reactor power and prevent damaging excursions when the system is

subjected to positive reactivity excursions. However, any event that tends to cool the fuel and moderator produces the opposite effect and tends to cause the reactor power to increase. Such accidents are brought about by overcooling the reactor inlet water for a variety of causes, overcooling of the feed water, failure of the atmospheric dump valve to close, inadvertent startup of coolant pumps which lowers the core average temperature. Figure 1 shows the excursion of relative neutron flux and fuel centerline hotspot temperature for a 42 degree Centigrade rapid drop in inlet temperature.

3.2.2 Loss of Flow

Another transient examined for potential damage was loss of flow due to the loss of one or more pumps with subsequent fuel over heating. Excursions of relative neutron flux and fuel centerline hotspot temperature resulting from a rapid 50 percent loss of flow are shown in figure 2.

3.2.3 Rod Ejection Accident

The Soviets have expressed concern about rod ejection accidents in the VVER-440's. This concern was brought about by the discovery of cracks at the point of connection of the control rod thimbles to the pressure vessel. Failure of this weld can cause the rod to be ejected from the reactor by the force of the primary system pressure. This failure mode bypasses the ejection velocity limiter designed to protect against failure of the seals. The individual rod worth may be as large as 1.3% $\Delta k/k$ in some operating configurations. The allowable regulating rod position as a function of power level is shown in Figure 19. These rods are withdrawn almost fully from the core during startup but may be inserted to the positions shown after the reactor reaches low power. The results of one rod ejection at 100% and 1%, respectively, were examined and found to have a minor effect. Two rod ejections were also examined for 100% and 1% starting power levels. The rationale for the two rod ejection cases is the increased probability that a second rod ejection might be triggered by a primary system pressure perturbation brought about by the first ejection. This was also found to have a minor effect.

3.2.4 Six Rod Run-away Accidents

Cases were studied for 6 rod cluster run-away events at full power. The design of the VVER-440 Model V213s utilize the redesigned rod drives. In these late model drives, a scram is effected by removing all AC power from the drive motors. This permits the rods to fall into the core under the influence of gravity. However, while falling into the core, the rod must drive the gear train in reverse and also spin the motor armature in reverse. The failure mode considered is that for some reason the scram fails to disconnect power from the drive motors and continues to respond to the control system withdraw signal. Six rods are considered here because the rods are driven in and out in groups of 6. The results of the analyses with the initiation of the run-away at 100% power and 1% power without scram are shown in figures 7 and 8, respectively. The possibility of fuel damage exists.

3.2.5 Excess Boron Removal Accidents

Finally, deboronation accidents were considered in which the removal of boric acid proceeded in an uncontrolled manner until criticality was reached. The possibility of fuel damage exists. The results of this case are shown in figure 9.

3.3 VVER-1000 Transients

The failures postulated in the FMEA give different results in VVER-440s and VVER-1000s due both to differences in reactivity coefficients shown in Table 1 as well as differences in failure modes for the two reactor systems. However, for completeness and for comparing the response of the two systems, similar

transients were run for both the 440s and 1000s. The results are given in figures 10 through 18. The results of these analyses were similar to those for the VVER-440s, i.e., transients with minor effects on VVER-440s had minor effects on VVER-1000s. Transients with the possibility of damage on VVER-440s had the possibility of damage on VVER-1000s. Figures 16 , 17, and 18 show these results. The incremental changes in the parameters are appropriate to the VVER-1000.

Further discussions of the reactivity control in VVERs can be found in Background Appendix C, "Reactivity Control".

Table 1 Key Parameters for the VVER-440s and VVER-1000s

PARAMETER	VVER-440	VVER-1000
Flow (Kg/s)	8700	22200
Thermal Power (Mw)	1375	3000
Coolant temperature reactivity coeff (\$/K)	-0.07	-0.100 to 0.015
Fuel temperature reactivity coeff (\$/K)	-0.005	-0.005
Active core fuel volume (m³)	4.39	7.55
Active core coolant volume (m³)	8.0	16.0
Ratio of hot channel power to average core power	1.55	1.46
Ratio of hot spot power to hot channel power	1.3	1.55

Table 2 Failure Modes and Effects (FMEAs) of VVER I&C Systems

Component	Failure Mode	Potential Causes	Effect of Plant	Remedial Actions
Regulating Rod Group	Uncontrolled withdrawal at full power	Failure of neutron detector for control	Power transient terminated by scram	Replace failed detector
Same	Same	Common mode failure of neutron detectors for control and safety	Power transient terminated by temperature or pressure scram.	Quality Control of sensor. Backup by diverse (in-core?) neutron sensor channels
Regulating Rod Group in VVER-440, Model 213.	Uncontrolled withdrawal from shutdown	Withdraw relay failure	Power transient terminated by scram.	Replace faulty relay.
Same	Same	Electrical short Safety actions thwarted by nature of failure.	Power transient.	Trip breaker which supplies control bus. Inject boric acid. Rewire, if necessary, so that AZ-1 emergency shutdown action interrupts current on both sides of the drive motor.
Single Regulating Rod Group	Rapid ejection from core	Seal failure in the jacket	Surge of power terminated by doppler, followed by period scram.	Inspect and maintain rod seals.

Table 2 Failure Modes and Effects (FMEAs of VVER I&C Systems (continued))

Component	Failure Mode	Potential Causes	Effect of Plant	Remedial Actions
Multiple Regulating Rod Groups	Rapid ejection from core	Common mode seal failures in the jacket, simultaneously initiated by pressure increase.	Surge of power terminated by doppler. Event terminated by period scram.	Inspect and maintain rod seals. Increase insertion speed of rods.
Chemical and volume control system	Uncontrolled boric acid concentration in coolant	Filling tank from wrong source. Failure of control valve. Incorrect concentration/dilution of boric acid.	Slow increase or decrease of boric acid in coolant. Power terminated by scram.	Strengthen administrative procedures. Repair replace faulty hardware.
Same	Boric acid fails to inject on demand	Faulty level indication on boric acid tank. Charging pumps fail to start. Control signal fails. Incorrect valve setting.	Reduced shutdown margin. Loss of backup reactivity control.	Manually start charging pumps on detection of failure if pumps are not failed mechanically. Initiate emergency boration alignment on detection of failure.

Table 2 Failure Modes and Effects (FMEAs of VVER I&C Systems (continued))

Component	Failure Mode	Potential Causes	Effect of Plant	Remedial Actions
Primary Coolant pumps	Startup of inactive coolant channel.	Operator action, either intended or inadvertent.	Surge in power due to negative plant temperature coefficient and influx of colder water. No damage anticipated. No scram required if power limitations observed before pump start.	Administrative procedures limit to 45 percent of rated power before restarting a pump with three other pumps running, and to 25 percent of rated power before restarting pump with two other pumps running. Hardware interlocks would be desirable backup.
Atmospheric dump valve (ADV)	Failure to close following opening	Loss of power to ADV.	Overcooling of system consequent to turbine trip.	Close isolation valves(s). Cut off coolant pumps.

Table 3 Summary of Results of RETRAN Reactivity Transient Analysis

	VVER-440	VVER-1000
<u>Transient</u>	<u>V-230</u>	<u>V-320</u>
1. Step Insertion		
Relative Power	1.86	1.89
Peak Clad Temperature degrees Fahrenheit	609	637
Pressure pounds/square inch	1801	2281
MDNBR	2.14	1.97
2. Ramp Insertion		
Relative Power	1.10	1.07
Peak Clad Temperature degrees Fahrenheit	610	633
Pressure pounds/square inch	1860	2357
MDNBR	2.16	2.14
3. Overcooling without Scram		
Relative Power	1.84*	2.70
Peak Clad Temperature degrees Fahrenheit	605	2270
Pressure pounds/square inch	1798	650
MDNBR	0.98	0.81

*** Estimated Value**

Table 4 Transients Analysed with Simplified Point Kinetics Model

<u>Transient</u>	<u>Reactor</u>	<u>Condition</u>	<u>Power</u>
1. Overcooling without scram	VVER-440	Inlet coolant temperature drops by 42 C.	100%
2. Loss of flow without scram	VVER-440	The flow steps are from 100% to 50%	100%
3. One rod ejection without scram	VVER-440	A 0.4% $\Delta k/k$ reactivity change is ramped in 50 milliseconds.	100%
4. One rod ejection without scram	VVER-440	A 1.34% $\Delta k/k$ reactivity change is ramped in 100 milliseconds	1%
5. Two rod ejection without scram	VVER-440	A 0.8% $\Delta k/k$ reactivity change is ramped in 50 milliseconds.	100%
6. Two rod ejection without scram	VVER-440	A 2.68% $\Delta k/k$ reactivity change is ramped in 100 milliseconds.	1%
7. Six rod runaway without scram	VVER-440	A 3 dollars reactivity change is ramped in approximately 100 seconds.	100%
8. Six rod runaway without scram	VVER-440	A 3 dollar reactivity change is ramped in approximately 100 seconds.	1%
9. Deboration without scram	VVER-440	With all control rods inserted, deboration is started, introducing 1.7 cents/second. Deboration is stopped when power reaches 105%.	1%
10. Overcooling without scram	VVER-1000	The inlet coolant temperature drops by 50°C.	100%
11. Loss of flow without scram	VVER-1000	The flow steps are from 100% to 50%.	100%
12. One rod ejection without scram	VVER-1000	A 0.4% $\Delta k/k$ reactivity change is ramped in 100 milliseconds.	100%
13. One rod ejection without scram	VVER-1000	A 0.75% $\Delta k/k$ reactivity change is ramped in 100 milliseconds.	1%
14. Two rod ejection without scram	VVER-1000	A 100 cents reactivity change is ramped in 100 milliseconds.	100%
15. Two rod ejection without scram	VVER-1000	A 100 cents reactivity change is ramped in 100 milliseconds.	1%
16. Six rod runaway without scram	VVER-1000	A 3 dollars reactivity change is ramped in 65 seconds.	100%
17. Six rod runaway without scram	VVER-1000	A 3 dollar reactivity change is ramped in 65 seconds.	1%
18. Deboration without scram	VVER-1000	With all control rods inserted, deboration is started, introducing 1.7 cents/second. Deboration is stopped when the power reaches 105%.	1%

42C DROP IN INLET TEMP - VVER 440

Figure 1 42°C Drop In Inlet Temp Without Scram - VVER-440

FLOW REDUCTION TO 50% - VVER 440

Figure 2 Flow Reduction to 50% in a VVER-440

ROD EJECTION @100% POWER - VVER 440

Figure 3 Rod Ejection at 100% Power - VVER-440

ROD EJECTION @1% POWER - VVER 440

Figure 4 Rod Ejection at 1% Power - VVER-440

2 ROD EJECTION @100% POWER - VVER 440

Figure 5 2 Rod Ejection at 100% Power - VVER-440

2 ROD EJECTION @1% POWER - VVER 440

Figure 6 2 Rod Ejection at 1% Power - VVER-440

6 ROD RUNAWAY @100% POWER - VVER 440

Figure 7 6 Rod Runaway at 1% Power - VVER-440

6 ROD RUNAWAY @1% POWER - VVER 440

Figure 8 6 Rod Runaway at 1% Power - VVER-440

BORON DILUTION @1% POWER - VVER 440

Figure 9 Boron Dilution at 1% Power - VVER-440

10C DROP IN INLET TEMP - VVER 1000

Figure 10 10°C Drop In Inlet Temperature - VVER-1000

FLOW REDUCTION TO 50% - VVER 1000

Figure 11 Flow Reduction to 50% - VVER-1000

ROD EJECTION @100% POWER - VVER 1000

Figure 12 Rod Ejection at 100% Power - VVER-1000

ROD EJECTION @1% POWER - VVER 1000

Figure 13 Rod Ejection at 1% Power - VVER-1000

2 ROD EJECTION @100% POWER - VVER 1000

Figure 14 2 Rod Ejection at 100% Power - VVER-1000

2 ROD EJECTION @1% POWER - VVER 1000

Figure 15 2 Rod Ejection at 1% Power - VVER-1000

6 ROD RUNAWAY @100% POWER - VVER 1000

Figure 16 6 Rod Runaway at 100% Power - VVER-1000

6 ROD RUNAWAY @1% POWER - VVER 1000

Figure 17 6 Rod Runaway at 1% Power - VVER-1000

BORON DILUTION @1% POWER - VVER 1000

Figure 18 Boron Dilution at 1% Power - VVER-1000

ANALYSIS APPENDIX I

FIRE PROTECTION

TABLE OF CONTENTS

	Page
1.0 Introduction	I-1
2.0 Summary of Fire Protection Related Soviet Requirements	I-1
3.0 Summary of Fire Protection Practices of U. S. Nuclear Power Stations	I-1
4.0 Fire Protection Features in Soviet Plants	I-2
5.0 Special Area of Discussion - Short Circuits	I-3

ANALYSIS APPENDIX I

FIRE PROTECTION

1.0 Introduction

This presents an overview of fire protection practices nuclear powerplants and discusses fire protection related design features in the Soviet VVERs. The review is based primarily on information contained in, DOE/NE-0084 "Overall Plant Design Descriptions VVER Water Cooled, Water Moderated Energy Reactor". Specific technical information needed to analyze details of fire protection considerations at VVER were not available.

2.0 Summary of Fire Protection Related Soviet Requirements

In general, fire protection has not been a priority consideration in the Soviet VVER and operating requirements. Specific requirements for the protection of systems needed to bring the plant to a stable post-fire shutdown condition do not appear to be required by the Soviet General Safety Regulations. The requirements used are relatively general and do not specifically address the consideration. These requirements (OPB-82, Soviet General Safety Regulations for Nuclear Powerplants during Design, Construction, and Operation) are:

"2.6.11 The capability of actuating the safety systems and of obtaining data about the status of the reactor from a standby control panel should be provided if for some reason (fire or so on) this cannot be done from the unit control panel."

"2.9.1 Support systems that perform the functions of supply of safety systems with a working medium and energy and that create conditions of their functioning during anticipated accidents should be provided."

"2.9.2 Support systems should perform their functions regardless of failure according to (OPB-82) item 1.2.4."

"3.6.4 The plan of measures should provide for coordination of the actions of operating personnel and of external organizations such as local authorities, fire-fighting companies, etc."

There are no requirements for physical separation of safety related or redundant cabling, or for any specific physical design features to prevent or mitigate the effects of fire.

3.0 Summary of Fire Protection Practices of U.S. Nuclear Power Stations

Nuclear powerplants in the United States are designed to satisfy General Design Criterion 3, "Fire Protection", of Appendix A to Title 10, Part 50, of the United States Code of Federal Regulations (10CFR50). This establishes the minimum requirements criterion to be used to judge the acceptability of the provision made to assurance that the facilities, structures, systems, and components important to safety are adequately protected from the effects of fire. In addition, Appendix R to 10CFR50 establishes specific fire protection design features necessary to meet General Design Criterion 3 of Appendix A to 10CFR50. Basically, each facility is required to establish a fire protection program that implements a philosophy of defense in-depth protection against the hazards of fire and its associated effects on equipment important to safety by:

1. Preventing fires from starting,
2. Rapidly detecting, controlling and extinguishing promptly fires that do occur, and
3. Providing protection for structures, systems and components important to safety so that a fire that is not promptly extinguished by the fire suppression activities will not prevent the safe shutdown of the plant.

Section III. G of Appendix R to 10CFR50 establishes specific design requirements for the fire protection of structures, systems and components important to safe shutdown. The design requirements imposed by this section include specific separation and protection features to ensure that at least one train of systems required to achieve and maintain hot shutdown conditions will remain free of fire damage and that systems necessary to achieve cold shutdown condition can be made available within 72 hours. For example, this section requires that redundant systems and equipment which are located outside of primary containment and necessary to achieve and maintain hot shutdown conditions be provided with at least one of the following fire protection design features:

- 3-hour, fire-rated barrier between the redundant trains of equipment.
- A minimum horizontal separation distance of 20 feet with no intervening combustibles with fire detection and automatic fire suppression installed in fire area.
- The enclosure of cable and equipment of one redundant train in a 1-hour, fire-rated barrier with fire detection and an automatic fire suppression system installed in the fire area.

Similar fire protection requirements are imposed for areas within reactor containments except when a containment is inerted and therefore without sufficient oxygen to support combustion. For those areas of the plant which do not comply with the protection requirements listed above, the facility may either seek an exemption from the specific technical requirements or provide an alternate shutdown capability independent of the cables, equipment and components in the fire area of concern.

4.0 Fire Protection Features in Soviet Plants

The Soviets consideration of the possible effects of fire in their safety regulations was discussed in Section 2 of this Appendix. However, information on the specific methods implemented to achieve compliance are not specified.

Section 2.6.11 of OPB-82, *General Safety Regulations of Nuclear Powerplants During Design Construction and Operation*, described in Section 2, requires alternate shutdown provisions. There appears to be only limited design provision made to provide for the electrical separation and/or isolation that would appear to be required between the unit control panel and the standby control panel to meet this requirement. It is not apparent that the systems and process instrumentation available on the standby control panel are electrically isolatable from the unit control panel such that the occurrence of fire initiated faults within this unit control panel would not affect the operation of the components on the standby control panel.

In addition, the following areas appear to be potentially significant in understanding the possible effects of fire damage:

- VVER-440 models V230s and V213s incorporate in their design a large annular ring that surrounds the reactor shaft and houses the motors for the primary coolant pumps and other electrical equipment. The corridor that leads to the annular ring is the electrical corridor through which the electrical, instrumentation and control cables are routed. Based on a review of the available drawings and computer generated views it appears that the corridor leading to this annular ring is a single, undivided area which contains cabling associated with redundant trains of control safety systems. A postulated fire in this area could result in damage to redundant trains of equipment needed to achieve safe shutdown conditions.
- There is a limited level of separation provided for the three diesel generators and associated control circuits. Although the diesel generators are reported to be isolated and independent of each other, it is not clear how assurance is provided that a single fire would not result in the loss of all diesel generators. In addition, no information is available concerning design provision for the electrical isolation of diesel generator control circuits from common areas such as the unit control room or computer room. While such design provisions may exist, the available information does not allow it to be identified.
- The computer room appears to be an area where all control and monitoring circuits are routed. A fire in this area may affect redundant systems required to achieve safe shutdown of the plant.
- It appears that reliance is placed on the use of external local fire fighting personnel. This is consistent with Section 3.6.4 of OPB-82 as described in Section 2 above. The Soviet regulations apparently do not require the formation of an internal fire brigade consisting of plant operating personnel. The response time and extent of training/plant familiarization provided for external fire fighting personnel remains a question.
- It is obvious that the specific cable routing and separation criteria for redundant trains of systems required to achieve post-fire safe shutdown were not located. The available information did not allow the identification of such redundant trains. As discussed in DOE/NE-0084, Revision 1, section 9.2.1., that the routes of cables from different compartments along their routes ensure that at least two channels will remain intact if one of the compartments experiences damage. Since the emergency protection system generates signals which initiate an automatic plant shutdown, it is interesting that the protection provided for such cables appears to have been specifically addressed document while the level of protection provided for cables associated with systems required to achieve safe shutdown either has not been considered, or the provision made was not addressed in available published information.

5.0 Special Area of Discussion - Short Circuits

VVER-1000 electrical system appear to have a heavy load on its associated 6 Kv system. According to "Fire Secure Station Service Electric Power for NPP with VVER-1000" by N. A. Fomin Enerpeticheshoe published in May 1986 (printed in Russian), several 6 Kv cables in these plants have a short circuit current of 25,000 amps available. The authors mention that the 150 mm² 6 Kv cables in question could see a temperature rise to at least 380 degrees centigrade and if the standby transformer were connected, these cables would see a "higher than permissible" temperature. The resultant recommendations by the authors to alleviate this potential fire concern is to put several large electrical loads on a separate 13.8 Kv supply system. This new dual voltage

recommendation would result in greatly reduced short circuit currents in the order of 110-150 degrees centigrade for each of the two systems.

None of the available information indicates that provision has been made for environmental qualifications of electrical systems. The features of electrical equipment (e.g. junction boxes, etc.) do not indicate provision for environmental qualifications. Provision to mitigate the effects of fires, or material used to fight fires on other electrical systems could not.

ANALYSIS APPENDIX J

AIRCRAFT IMPACTS

TABLES

1. **Potential Consequences of Aircraft Crashes at a VVER Site Assuming Penetration of Structural Barriers**
2. **Characteristics of Selected Soviet Military Aircraft**

ANALYSIS APPENDIX J

AIRCRAFT IMPACTS

1.0 Introduction

This appendix considers the potential consequences that could result from the crash of an aircraft at a VVER site. This assessment does not contain independent structural analyses, but rather applies the results of published structural analyses of the impact of aircraft upon containment structures.

The consequences of an aircraft crash at a nuclear power station are dependent upon the precise crash location. Table 1 lists some of the potential consequences of a crash occurring at different areas within the boundary of a VVER site, assuming effective penetration of all structural barriers. An assessment of the penetration potential for the impact of a specified reference aircraft upon actual VVER buildings is presented below.

Penetration of the accident localization volume boundary of a VVER-440 or the containment boundary of a VVER-1000 can have implications for transboundary releases, should the ability to effectively remove the core residual heat be compromised by the crash-induced damage. However, crashes at other locations may also have serious implications. For example, an aircraft crashing into the turbine building might result in the escape of secondary side coolant and a loss of heat sink condition impacting upon the ability to remove the core decay heat. VVER units do not have dedicated residual heat removal systems providing a redundant residual heat removal capability distinct from the normal secondary side. Consequently, an aircraft crash-induced loss of heat sink event assumes a major significance. Similarly, destruction of the pumping station could bring about a loss of service water as well as the condenser cooling water which constitutes the ultimate heat sink.

Aircraft impact is not a design basis for any currently operating VVER. However, the crash of an aircraft with a mass of 20,000 kilograms (44,000 pounds) and a velocity of 200 meters per second (720 kilometers per hour equal to 450 miles per hour) is included in the design of the VVER-440 units under construction in Cuba.¹ Although aircraft impact is not part of the design bases of the VVER-1000 model V320, the Soviets have performed analyses of the consequences of aircraft crashes into the containment.² Also, an aircraft crash is a design basis for the ATETS VVER-1000 district heating plant design.

The mass of 10,000 kilograms (22,000 pounds) and velocity of 208 meters per second (750 kilometers per hour equal to 466 miles per hour) corresponds to a military aircraft such as the Mig-21 at cruising speed.

The Soviet design considerations are similar to the practice followed in the Federal Republic of Germany (FRG). In the FRG, the crash of a military aircraft at cruising speed is a design basis accident.³ The FRG requirements instituted in the early 1970s are more stringent in that impact upon areas containment shell must be addressed. In general, buildings which incorporate systems required for the safe removal of residual heat and which cannot be provided by a redundant capability must be protected against crashes. The specific impact loading has been stipulated to be that corresponding to the crash of a, then-current, multirole combat aircraft. Originally, the crash of an F-104 Starfighter jet fighter was assumed. This requirement is reflected in the design of the Neckarwestheim, Ohu-1, Unterweser, Biblis-B, and THTR-300 units.⁴ More recent German units have been designed on the basis of the impact of the larger and heavier RF-4E Phantom.³ The F-4E has an empty weight of 14,000 kilograms (31,000 pounds) and a maximum gross weight of the order of 26,000 kilograms (58,000 pounds). The West German requirements specify the impact of a mass of 20,000 kilograms (44,000 pounds) at a velocity of 215 meters per second (481 miles per hour). The Krümmel, Kalkar, Muelheim-Kaerlich, Grohnde, Philippsburg-2, Brokdorf, Isar-2, Gundremmingen-B, Gundremmingen-C, and Grafenrheinfeld units, (ten in all), were designed according to the F-4 impact requirement.⁴

In addition to the shocks induced by the impact itself, the design is also required to make allowance for the effects of fragments and fires resulting from the burning of kerosene fuel.³ While reinforced concrete structures are well suited to withstand fire, measures must be provided to prevent fire, smoke, or flammable mixtures from entering the ventilation intakes to safety-related areas. Examples are the control room, areas containing equipment necessary to shut down the reactor, and the emergency diesel generators.

The impact loading resulting from the crash of a military aircraft such as an F-4 is generally more severe than the impact of a large commercial airliner. In particular, a multirole combat aircraft is more compact, does not have a large fuselage capable of absorbing relatively more deformation energy, and characteristically flies with greater speeds when at low altitudes.⁵ In contrast, commercial airliners carry larger quantities of fuel which can result in more extensive fire-induced damage. For example, the effects of the crash of a commercial airliner into the switchyard would likely be more widespread than the effects of the crash of a multirole combat airplane. The crash of a commercial airliner is employed as a design basis in Belgium and Switzerland.⁶

Aircraft impact is also a design basis accident in France.³ Buildings containing systems important to safety must be protected from the crash of general aviation aircraft. Two specific reference planes are considered. The first is a Lear Jet 23 having a weight of 5,700 kilograms (12,600 pounds), a velocity of 100 meters per second (224 miles per hour), and impacting over a surface area of 12 square meters (130 square feet). The second plane is a Cessna 210 with a weight of 1,700 kilograms (3,750 pounds), a velocity of 100 meters per second (224 miles per hour), and an impact area of 4 square meters (43 square feet). Crashes of military and commercial aircraft are ruled out in France on the basis of low probabilities of occurrence.

In the United States, crashes have also been ruled out on probabilistic considerations, except for sites located near commercial airports or military bases.³ Normally, aircraft effects are not considered in the United States, unless the plant is situated within an approximately 8 kilometer (5 mile) radius of an airport or air base.³ For example, the Three Mile Island station was constructed 4 kilometers (2.5 miles) from the Olmsted State Airport and 13 kilometers (8 miles) from the Harrisburg Airport. The plant was designed to protect the containment, engineered safety features, and safe shutdown equipment against the crash of a 91,000 kilogram (200,000 pound) Boeing 720 commercial airliner over a circular area of 25 square meters (280 square feet)⁷ with a velocity of 103 meters per second (230 miles per hour) and against fires resulting from the crash. Specific structures designed to the aircraft impact criteria are the reactor building, control and service buildings, auxiliary and fuel handling buildings, control building area, river water pump house, and the below-ground portion of the air intake tunnel.⁷

2.0 Results of Analyses of Aircraft Impacts Upon Nuclear Structures and Equipment

The impact of a military aircraft crash upon thick-walled nuclear structures has been found to have three principal effects. First, there may be local failure of the structure within the impact area resulting in penetration. Second, the turbine engines have a relatively large weight to volume ratio and therefore a high penetration capability. If an engine penetrates the concrete shell, the potential exists for heavy fragments to impact upon equipment and piping. Third, the impact induces vibrations in the range of 20 to 80 Hertz which can cause the failure of equipment.

Previous studies have indicated that aircraft impact is a local phenomenon contained within a radius of about five meters.⁵ Outside of 5 meters, deformations were predicted to be small. The largest response would generally result when the aircraft impacts the surface of the structure. One study⁵ assuming the impact of an F-4E at a velocity of 215 meters per second indicated that a containment (20 meter radius and 64 meter height) with a dome thickness of 1.2 meters will fail under a vertical straight downward impact on the dome. However, a dome with a thickness of 1.8 meters was calculated to withstand the aircraft impact. Another study⁸ indicated that a PWR containment (32 meters radius) in Switzerland designed for the crash of a Boeing 707 at the speed of 103 meters per second requires a dome thickness of 1.2 meters (3.9 feet). Note that a Boeing 707 would have a lower penetration potential than a military

aircraft. Another example is a containment with a 25 meters radius in Belgium.⁹ It was also designed for the impact of a Boeing 707 at a speed of 102 meters per second. The thickness in this case is 1.0 meter for the dome and 1.2 meters for the wall. An aircraft impact analysis¹⁰ performed in the United Kingdom for the impact of an F-4 Phantom as well as a Tornado upon a cylindrical containment (54 meters height, 45 meters diameter, 1.5 meters thick dome and 2.5 meters thick cylindrical wall) indicated that the liner in the local area would rupture. The liner studs in the local area under impact load were calculated to rupture. In other areas, the liner was predicted to have buckled.

A Finnish investigation¹¹ analyzed the downward directed impact of an RF-4E Phantom upon the apex of a containment with a hemispherical dome. The dome had an outer radius of 24 meters (79 feet) and a nominal thickness of 1.5 meters (4.9 feet), identical to the dimensions of the dome of the Cuban VVER-440 variant. In addition, the amount of steel reinforcement was initially assumed to correspond to a value of 80 kilograms per cubic meters (5 pounds per cubic foot) which is representative of the steel densities employed in the 1.5 meters (4.9 feet) thick walls of the VVER-440 model V213 and is also expected to be characteristic of the Cuban units. A greater amount of reinforcement corresponding to 200 kilograms per cubic meters (12 pounds per cubic foot), more representative of Western practices, was also considered. A layer of rebar was assumed to be located 0.1 meters (0.33 foot) away from each of the shell inner and outer surfaces. For both rebar densities, failure was predicted. For a rebar presence of 200 kilograms per cubic meters (12 pounds per cubic foot), the shell thickness was increased in a parametric fashion to 1.8 meters (5.9 feet) and 2.1 meters (6.9 feet). It was concluded that a minimum wall thickness of 2.1 meters (6.9 feet) is required to preclude failure and penetration.

Calculations were performed in the FGR of the response of a 1.5 meter (4.9 foot) thick reinforced concrete slab to an impact loading representative of an F-4E crash.¹² Failure of the slab was predicted. It was concluded that in the absence of a significant enhancement of the reinforcement, an increase of the concrete thickness to a value of 1.8 to 2.0 meters (5.9 to 6.6 feet) was necessary, to respond to the FGRs impact loading requirement.

Finally, a Swiss analysis¹³ modeled the horizontally directed crash of an RF-4E Phantom into a ventilation stack having a thickness of 0.25 meters (0.82 feet) and an outer diameter which decreases from 8 meters (26 feet) to 5 meters (16 feet) over its 100 meters (330 feet) height. Impacts near the base, middle, and top were examined. A number of calculations were run to encompass uncertainties in material properties. In most cases, the stack was predicted to collapse due to rupture of the stack over its annular cross section or by overturning caused by the impact-induced moment forces. The subsequent fall of the stack upon the dome of a neighboring containment shell was found to result in loadings of similar magnitude and duration to those used to characterize the crash of a Boeing 707-320 commercial airliner.

The results of published analyses indicate that a minimum thickness of reinforced concrete of 1.8 to 2.1 meters (5.9 to 6.9 feet) is required to withstand the impact of a 20,000 kilogram (44,000 pound) multirole combat aircraft such as the F-4 at a velocity of 215 meters per second (481 miles per hour).

An aircraft crash is typically accompanied by the release of large quantities of fuel. The fuel constitutes a significant portion of the aircraft gross weight. Jet fuels are not highly volatile, but will readily burn and can explode when mixed with air in suitable proportions. A fire is therefore likely following a crash and an explosion is a possibility. The fire may burn for a period of time of the order of minutes or tens of minutes. Hot combustion products, principally gaseous in nature, will rise upward due to buoyancy forces and will be transported downwind. The hot combustion products can disrupt equipment and personnel in neighboring areas.

The published analyses have generally considered the direct impact of an aircraft, often vertically downward, upon thick-walled structures. However, an airplane crashing into the ground may subsequently skid along the ground for a considerable distance before coming to rest. For example, an aircraft which crashes into the switchyard and then skids along the ground may damage a large area of equipment in its path.

3.0 Impact Loads

A specialized analysis of aircraft impact upon a particular VVER structure requires complex, dynamic, nonlinear structural calculations. The team did not perform such an analysis. Instead, an assessment of the effects of a crash upon various VVER buildings was carried out by drawing upon the results of the Western calculations discussed above. This approach was an interpolation or extrapolation of the results of analyses performed by others. As noted above, a reinforced concrete thickness of 1.8 to 2.1 meters (5.9 to 6.9 feet) is indicated as the minimum necessary to resist the crash of a 20,000 kilogram (44,000 pound) aircraft at 215 meters per second (481 miles per hour). Implicit in the previous analyses are the construction practices followed in Western countries. Since Soviet construction practice generally employs less reinforcement and lower steel masses for a given amount of concrete, somewhat greater thicknesses may even be required to resist the same impact load upon a VVER accident localization volume or containment.

A reference aircraft impact loading corresponding to a mass of 20,000 kilograms (44,000 pounds) and velocity of 215 meters per second (481 miles per hour) is assumed for the present assessment. Characteristics of relevant Soviet manufactured jet military aircraft are shown in Table J-2.¹⁴ The reference impact conditions correspond suitably well to the crash of a Mig-23 or Mig-27 Flogger. While gross weights of 16,000 kilograms (35,000 pounds) and 17,000 kilograms (37,000 pounds) are specified in reference 13, a maximum loaded weight of 19,000 kilograms (41,000 pounds) is quoted for the Mig-23 and 20,000 kilograms (44,310 pounds) for the Mig-27 in reference 15.

The assessments of the potential for penetration of reinforced concrete structures do not include the effects of the explosion of jet fuel which can result in pressure wave loadings upon the structures. Also, if a military aircraft is carrying ordnance, the crash may involve blast waves caused by the detonation of high explosive. In such a case, the additional blast loadings can significantly increase the penetration potential as well as the extent of the damage.

4.0 Aircraft Impact Upon the Reactor Building

4.1 VVER-440 Model V230

The primary system components are located inside a compartment (called an accident localization volume) that has steel-lined reinforced concrete walls and roof that are approximately 1.0 meter (3.28 feet) thick. The overlying reactor bay is an industrial-type structure having a framework of reinforced concrete columns together with walls and a roof consisting of prefabricated reinforced concrete slabs. Downward directed reference aircraft impact is expected to readily penetrate through the roof of the reactor building to impact with considerable kinetic energy upon the operating deck floor forming the roof of the accident localization volume. If the impact area lies above the localization volume, then the aircraft could be expected to penetrate through the operating deck floor into the localization volume. Any of the potential consequences of a crash into the reactor building in Table J.1 may be realized, depending upon the precise impact location. Penetration will generate heavy fragments which will impinge upon primary system components and primary piping; this could initiate a loss-of-coolant accident. Steam or feedwater piping may be ruptured as well thereby initiating a concurrent secondary side loss-of-coolant accident. Makeup system piping may also be damaged, degrading or precluding the addition of makeup water to the primary coolant system. If sufficient coolant cannot be delivered to the vessel to remove the core decay heat, core degradation and meltdown will occur.

If impact occurs on the refueling pool, the pool walls will likely be breached allowing water to drain from the pool volume possibly precluding the cooling of spent fuel assemblies which might reside inside the pool. Spent fuel assemblies may also be directly damaged by debris fragments resulting in the release of fission products. If the aircraft impacts upon the 2.5 centimeter (0.98 inch) thick hemispherical dome above the reactor vessel, the dome will be readily penetrated and the control assembly drive mechanisms (that are located above the vessel upper head) will be damaged.

This could initiate a loss-of-coolant accident in the upper head region. Cables routed to the control drive mechanisms will probably be damaged or severed.

If impact occurs over the central region between the two reactor units, then damage may occur to the fuel handling facilities used to transport fuel into and out of the reactor building. In all cases, there may be fires on the operating deck or inside the localization volume. Loss of electrical power may occur due to the effects of fire as well as debris fragments.

If impact does not occur upon the roof or walls of the reactor bay, the consequences would be dependent upon the crash location. Penetration could occur into the control and auxiliary equipment section surrounding the two accident localization volumes of the two reactor units in the bay and the reactor bay. Depending upon the impact point, complete destruction of the control room of one of the units may occur. Because the control room for the other unit is located at the opposite end of the reactor building, the control room of the other unit would not be damaged. There is no indication that measures are provided to protect the control room from the effects of fires following an aircraft crash. Consequently, significant damage to the control room could occur even if the airplane does not penetrate directly into the control room but crashes nearby instead. If destruction of the backup control room occurs, then the redundant capability to shut down the reactor will be lost. The control and auxiliary equipment section houses ventilation equipment. If damage to the ventilation equipment serving one of the accident localization volumes occurs, precluding adequate heat removal or filtering, then shutdown of the affected unit would be required. Steam and feedwater lines for each unit pass between the accident localization volume and the turbine building through the intervening part of the control and auxiliary equipment section. Depending upon the precise crash location, the rupture of steam or feedwater piping may occur initiating the loss of secondary coolant and depressurization of the secondary system.

4.1.2 VVER-440 Model V213

For the reference impact conditions, the aircraft could readily penetrate through the reactor building roof to impinge upon the operating deck. The reinforced concrete walls and overlying operating deck of the VVER-440 model V213 accident localization volume have a thickness of approximately 1.5 meters (4.9 feet). This is less than the minimum thickness of concrete necessary to resist penetration as indicated by the calculations discussed above concerning impacts upon containments. Consequently, penetration of the operating deck floor could be expected to occur. Again, the potential consequences enumerated for a crash into the reactor building in Table J-1 may be realized, depending upon the precise point of impact. Penetration through the operating deck floor could generate heavy fragments which will could impinge upon primary system components and piping and initiate a loss-of-coolant accident. Steam or feedwater piping could also be ruptured initiating a simultaneous loss of secondary coolant. The accumulators and emergency core cooling system piping could be damaged. Inability to deliver sufficient make-up emergency coolant to the core to compensate for that escaping through breaks in the primary system boundary may result in core uncover, degradation, and meltdown. Makeup system piping may also be ruptured degrading or preventing the addition of water to the primary system through the makeup flow. If impact occurs on the refueling pool, the pool walls will likely be breached allowing water to drain from the pool possibly precluding the removal of decay heat from spent fuel assemblies stored in the pool. Debris fragments may also directly damage spent assemblies residing in the pool thereby giving rise to the release of fission products. If the aircraft impinges upon the protective cap overlying the reactor vessel, then the cap will be readily penetrated and damage to the control assembly drive mechanism above the reactor pressure vessel occurs. This could initiate a loss-of-coolant accident in the upper head region with steam and fission products escaping through the failed cap region. Control cabling from the upper drive assembly could be damaged or severed. In all cases, fires may be present on the operating deck or inside the accident localization volume. Fires and debris fragments may cause a loss of electrical power inside the reactor building (Analysis Appendix I, "Fire Protection").

If impact occurs upon one of the bubbler-condenser towers, then the 1.5 meters (4.9 feet) thick roof or walls of the bubbler tower will be penetrated by the reference aircraft. Penetration of the tower roof or walls represents breaching of the accident localization volume boundary. In the event of a primary coolant system rupture, steam and fission products could escape through the breach.

If impact occurred in locations other than the roof or walls of the reactor bay, the consequences would be dependent upon the crash location. Penetration would occur into the control and auxiliary equipment section surrounding the two accident localization volumes and the reactor bay. Depending upon the impact point, complete destruction of the control room of one of the units could occur. Because the control room for the other unit is located at the opposite end of the reactor building, the control room of the other unit would not be damaged. There is no indication that measures are provided to protect the control room from the effects of fires following an aircraft crash. Significant damage to the control room could occur even if the aircraft does not penetrate directly into the control room but crashes nearby instead. If destruction of the backup control room occurs, then the redundant capability to shut down the reactor could be lost. The control and auxiliary equipment section houses ventilation equipment. If damage to the ventilation equipment serving one of the localization volumes occurs precluding adequate heat removal or filtering, then shutdown of the affected unit would be required. Steam and feedwater lines for each unit pass between the accident localization volume and the turbine building through the intervening part of the control and auxiliary equipment section. Depending upon the precise crash location, the rupture of steam or feedwater piping could occur initiating the loss of secondary coolant and depressurization of the secondary system.

4.1.3 VVER-1000 Model V320

VVER-1000s have a prestressed, reinforced concrete cylindrical containment with an elliptical dome. The cylindrical shell and dome have concrete thicknesses of 1.2 meters (3.9 feet) and 1.1 meters (3.6 feet) respectively. The shell inner surface is lined with carbon steel 8 millimeters (0.315 inch) thick. For a downward directed impact upon the dome, the aircraft could be expected to penetrate both the concrete and steel liner thicknesses. Heavy concrete fragments from the dome, aircraft engine parts, and other debris fragments could impinge upon the operating floor at the 36.9 meters (121 foot) level or upon the reactor assembly. The upper part of the reactor assembly including the vessel upper head and the control rod drive assembly is not covered with any protective structure. Thus, damage to the upper drive assembly could occur, initiating a loss-of-coolant accident. Control cabling from the upper assembly could be damaged or severed. Debris fragments could directly damage spent fuel assemblies inside the refueling pool resulting in the release of activity. Penetration of the approximately 1 meter (3.28 feet) thick operating floor appears to be unlikely to occur, since most of the aircraft crash energy would be absorbed by the containment shell and operating floor structures. No significant damage would be expected from the direct impingement of debris upon the components and piping located beneath the operating floor. However, the primary system components and piping as well as other equipment will still be subjected to impact-induced vibration which may cause damage. A fire may occur inside the containment may cause a loss of electrical power.

For impacts on location other than the containment shell, the consequences would be dependent upon the crash location. Penetration could occur into the square control and auxiliary equipment shield building surrounding the containment. The control room is located near the outer wall of the shield building adjacent to the turbine building. Depending upon the impact point, destruction of the control room could occur. There is no indication that measures are provided to protect the control room from the effects of fires resulting from the crash of an aircraft. Significant damage of the control room might result from fire even if the aircraft debris does not penetrate directly into the control room. A crash into the shield building (the structure that surrounds the containment building in the VVER-1000 model V320) may damage ventilation equipment serving the reactor building and housed in upper levels of the shield building. Steam and feedwater piping pass between the containment and turbine buildings through an upper level of the shield building. Depending upon the precise crash location, rupture of a portion or all of the steam and feedwater pipes may occur resulting in the loss of secondary coolant and depressurization of the secondary side.

5.0 Aircraft Impact Upon the Turbine Hall

5.1 VVER-440 Models V230 and V213

The turbine hall of all VVER-440s are industrial-type buildings having roofs and walls which could be readily penetrated by the reference aircraft. The consequences of a crash into the turbine building are strongly dependent upon the precise impact location. Except for the VVER-440 model V213 units under construction at Zarnowiec in Poland, all VVER-440s use two turbine generators per unit. This twin generator configuration was used in the current analysis. The layout of the secondary system components and their dimensions are such that it is possible for an aircraft crash to disable part of the secondary side associated with one turbine generator while the portion connected with the other remains operational. Availability of the heat transport path through one turbine will preclude a complete loss of heat sink condition. However, there are critical components which are common to both turbine generators, such as the feedwater pumps. Loss of the feedwater pumps, together with the emergency feedwater pumps, could initiate a complete loss of heat sink accident.

For each unit, the main steam lines entering the turbine building are physically separated into two groups, each corresponding to the three steam generators located on one side of the accident localization volume. If the crash damages steam piping belonging to only one group while the other group as well as the other components and piping remain intact, a partial loss of heat sink event could be initiated. Rupture of steam piping could cause depressurization of the affected half of the secondary side and loss of secondary coolant. The depressurization could enhance the primary-to-secondary heat transfer which could cool down the primary system. The severity of the primary system cooldown transient would be expected to increase with the number of steam lines undergoing rupture. Similar considerations hold for the feedwater piping exiting the turbine building in two groups of pipes routed to the two sides of the accident localization volume.

Rupture of feedwater pipes could also result in secondary side depressurization, loss of secondary coolant, primary system cooldown, and partial or complete loss of heat sink dependent upon the crash location. In specific places, feedwater pipes are located in close proximity to the steam pipes such that concurrent damage of both steam and feedwater lines could occur. If one of the turbines is damaged, the consequence would be a partial loss of heat sink. Damage to one or both of the bottom-mounted condensers can result in a loss of the condenser cooling water as well as secondary coolant for the particular turbine. Damage to both condensers in VVER-440 model (V230s) could lead to release of radioactive material since these units do not have an auxiliary feedwater system or independent supply of cooling water. Loss of the secondary system condensate removes the ultimate heat sink and leaves no way to cool the primary system except by boiling away primary coolant. If the crash caused the loss of the generator without damaging secondary system components, a turbine trip will be necessary. Damage of only one or both of the separator-reheaters could cause loss of secondary steam and initiate a loss of heat sink condition.

Loss or damage of a portion or all of the four condensate pumps located beside each turbine generator could result in less than adequate flow of water through the low pressure heaters and inadequate delivery of water to the deaerator as well as loss of secondary coolant and partial loss of heat sink. Similarly, damage of one or more of the low pressure heaters and associated piping could cause loss of secondary coolant. The heat transport path associated with each turbine generator incorporates its own deaerator. A partial or complete loss of heat sink could occur depending upon whether one or both of the deaerators are damaged.

All of the feedwater pumps for each unit are located side-by-side and simultaneously serve to recirculate the fluid from the two deaerators associated with the two turbines. The auxiliary feedwater pumps in VVER-440 MODEL V213s are located near the main feedwater pumps. A crash which results in the loss of all of the feedwater pumps as well as the auxiliary feedwater pumps would initiate a total loss of heat sink. The feedwater pumps and auxiliary feedwater pumps

thus represent critical components which are essential to the heat transport paths connected with both turbines. Fluid from the feedwater pumps passes through the high pressure heaters, three of which are provided for each turbine. Damage of one or more high pressure heater will cause loss of secondary coolant and a partial loss of heat sink. If the aircraft crash ruptures any of the condenser cooling pipes leading to the condensers under one turbine, a partial loss of ultimate heat sink could occur.

Damage of the oil pumps, oil coolers, oil piping, or the oil tank for one turbine will preclude continued operation of the turbine generator in the absence of damage to other secondary components. In addition, the oil provides a source of fuel for fire. In all cases, fires could occur inside the turbine building. Fire and debris could cause a loss of electrical power. Because the condensate pumps, feedwater pumps, and auxiliary feedwater pumps are all electrically driven, extensive loss of electric power due to the destruction of electric wiring may result in a partial or complete loss of heat sink. In addition, electric power loss might affect both units depending upon the wiring layout, and the location of the aircraft impact.

5.2 VVER-1000 Model V320

The VVER-1000 model V320 turbine hall is an industrial-type building with a roof and walls which could be readily penetrated by the reference aircraft. The consequences are strongly dependent upon the precise crash location. Each VVER-1000 model V320 unit has a single turbine generator housed in separate buildings. Consequently, an aircraft crash can cause a complete loss of heat sink as a result of damage to any of a number of components. However, there are portions of the secondary heat transport loop which involve parallel paths such that partial loss of heat sink events could result. The steam lines from the four primary loops enter the turbine building in a relatively localized region. Thus, it is possible for a crash to result in the rupture of from one to all four of the steam pipes, causing depressurization of the secondary system and the loss of secondary coolant. The depressurization of the secondary side caused by steam line rupture will increase the primary-to-secondary heat transfer tending to lower the temperature of the primary coolant. The severity of the primary system cooldown transient is expected to increase with the number of steam lines ruptured.

Loss of secondary coolant would cause a partial or complete loss of heat sink. Similar considerations hold for rupture of the feedwater pipes which leave the turbine hall in close proximity to the steam pipes. Damage of the feedwater piping would result in secondary system depressurization, loss of secondary coolant, and cooldown of the primary system. Because they are located in close proximity in specific places, a crash could cause the simultaneous rupture of both steam and feedwater lines. Use of a single turbine means that loss of the turbine could result in a complete loss of heat sink. Damage to one or more of the underlying basement mounted condensers would result in loss of condenser cooling water and a partial or complete loss of ultimate heat sink. If one or more of the four separator-reheaters is damaged, the consequence could be loss of secondary steam and loss of heat sink.

Two condensate pumps, each of which consists of three pumps in parallel, are located beside the turbine generator. If a sufficient number of pumps are damaged, then inadequate coolant would be delivered to the low pressure heaters and the deaerators potentially resulting in a loss of heat sink event. The four low pressure heaters consist of a number of separate shells beside the turbine generator. Damage of one or more low pressure heater could permit the escape of secondary coolant and initiate a loss of heat sink.

VVER-1000 model V320s incorporate two deaerators. Generally, two parallel paths exist for the secondary coolant beginning with the deaerators and ending at the feedwater lines which ultimately leave the turbine building. The deaerators are located in a separate part of the turbine hall away from the turbine generator, condensate pumps, low pressure heaters, and high pressure heaters. If the aircraft impacts upon the deaerator section and one deaerator is damaged but the other remains intact, then a complete loss of heat sink would not occur, if the damaged deaerator was isolated. The deaerators are located one floor level below the two turbine-driven feedwater pumps. The feedwater

pumps draw fluid from the deaerators. If only one pump is damaged and a viable heat transport path exists using the second pump, then a complete loss of heat sink would not occur. Because the feedwater pumps are located beneath the deaerators, concurrent loss of deaerating and feedwater capabilities are a possibility. The secondary coolant flows from the feedwater pumps to the three high pressure heaters. Each high pressure heater consists of two shells each of which belongs to one of two parallel trains. If heaters belonging to only one train are affected, then a complete loss of heat sink might not occur. However, all of the heater shells are located in close proximity to each other and damage to the shells in both trains could occur simultaneously, depending upon the crash impact point. Rupture of a portion of the condenser cooling pipes could result in a partial loss of the ultimate heat sink.

Damage of the oil pumps, oil coolers, oil piping, or the oil tank could preclude continued operation of the turbine generator. The oil constitutes a fuel source for fires. If the crash damages the generator without damaging the secondary components, then a turbine trip will be necessary. In all cases, a fire may occur inside the turbine hall. Fire and debris could cause a loss of electrical power in the turbine hall, depending upon the layout of the electric wiring. An extensive loss of electrical power could result in a partial or complete loss of heat sink. However, an uncertainty here is the continued operation of the feedwater pumps, each of which is driven by its own steam turbine should mitigate the impact of this event.

6.0 Aircraft Impact Upon the Radwaste and Workshop Building

6.1 VVER-440 Models V230 and V213

The VVER-440s incorporate a common radwaste building for the two units belonging to each module. The reference aircraft would penetrate the radwaste building roof or walls. The consequences would be dependent upon the precise crash location. For the VVER-440 models V230 and V213, components of the primary coolant purification system are housed inside the radwaste building. Damage to purification system components or piping for one unit would likely preclude continued operation of the reactor. Liquid wastes are stored in cylindrical tanks inside the radwaste building. If debris ruptures one or more tanks, release of liquid activity may occur. Damage to components used to extract and concentrate wastes such as evaporators and ion exchange units will impact upon waste handling activities. If equipment used to remove radioactive aerosols and gases from the accident localization volume is damaged, shutdown of a reactor may be required. The radwaste building contains workshops and supporting personnel facilities for the repair and maintenance of active components. A crash into the workshop or personnel portion of the building could impact upon repair, maintenance, or refueling activities. In all cases, fire and loss of electric power could occur inside the radwaste building.

6.2 VVER-1000 Model V320

VVER-1000 model V320s incorporate a single radwaste building serving all of the VVER-1000 model V320 units located on the site. The reference aircraft would penetrate through the radwaste building roof or walls. The consequences would be dependent upon the precise crash location. If piping or components involved in the processing of wastes extracted from the primary purification systems of the units were damaged, then the continued operation of one or more of the reactors could be precluded. A large portion of the radwaste building houses cylindrical tanks used to store liquid wastes. If the crash results in the rupture of one or more tanks, release of liquid activity could occur. The radwaste building contains workshops and supporting personnel facilities for the repair and maintenance of active components. A crash into the workshop or personnel portion of the building would impact upon repair, maintenance, or refueling activities. In all cases, fire and loss of electric power could occur inside the radwaste building.

7.0 Aircraft Impact Upon the Pumping Station

The pumping station, also referred to as the water intakes, provides cooling water for the turbine condensers, water for the intermediate cooling system, and service water. The water delivered to the condensers constitutes the ultimate heat sink for the energy removed from the reactor core. Consequently, the inability to deliver cold water to the condensers will result in a loss of ultimate heat sink accident.

7.1 VVER-440 Models V230 and V213

VVER-440s generally employ a common pumping station for the two units belonging to a plant. The reference aircraft could readily penetrate through the roof or walls. When the pumps and pump motors are located inside a common building and are not separated by fire retardant walls. A concurrent loss of ultimate heat sink for both units could occur. If the crash results in the loss of only a portion of the pumping capacity, then a partial or total loss of ultimate heat sink might result. In all cases, a fire could occur inside the pumping station. Fire and debris could cause a loss of electrical power inside the pumping station, depending upon the specific wiring layout. Extensive power loss could disable all of the pumps resulting in a total loss of ultimate heat sink for both units.

7.2 VVER-1000 Model V320

Each VVER-1000 model V320 unit is provided with its own pumping station. The individual pumping station buildings are constructed side-by-side and are physically separated from each other in the same manner as the reactor and turbine buildings of the various units. Consequently, a crash upon the pump house of one unit would not be expected to affect neighboring units. The reference aircraft could readily penetrate through the pumping station roof or walls. It is not known what provisions exist to use the pumps belonging to one unit as a backup for other units. If suitable interconnections do not exist, then an aircraft crash into the pumping station of one unit could initiate a total loss of ultimate heat sink for that unit. Even if provisions for interconnection of water lines do exist, the VVER-1000 model V320 units are constructed in series such that only the first unit may be operational while potential alternative pumping capabilities associated with other units have not been completed. Depending upon the precise crash location, only a portion of the pumps might be damaged precluding a total loss of ultimate heat sink. In all cases, a fire could occur inside the pumping station. Fire and debris could initiate a loss of electrical power in the pump house. Extensive loss of power could disable all of the pumps thereby causing a complete loss of ultimate heat sink.

8.0 Aircraft Impact Upon the Cooling Water Inlet Canal

This is the canal that delivers cold water to the pumping station from which it is piped to the turbine condensers, intermediate cooling system, and service water system. The canal is typically wide with concrete walls and bottom. At specific VVER sites such as Kozloduy in Bulgaria and Armenia in the Soviet Union, the canal walls consist of prefabricated reinforced concrete slabs. The reference aircraft could readily penetrate through the walls of the canal potentially permitting water to drain from the canal into the surrounding soil. Inability to draw water to the pumps would result in a total loss of ultimate heat sink for all of the units on the site.

9.0 Aircraft Impact Upon a Cooling Tower

Specific VVER plants employ cooling towers to reject heat removed by the condenser cooling water to the atmosphere. An aircraft crash would not be expected to damage more than one tower due to the large size of an individual tower unit. Consequently, a crash into a cooling tower would result in a partial loss of the ultimate heat sink.

10.0 Aircraft Impact Upon a Spray Pond

Specific VVER sites employ spray ponds to reject heat removed by the condenser cooling water to the atmosphere. An example is the six-unit VVER-1000 model V320 station at Zaporozhye. Due to the large dimensions of the spray ponds, only a partial loss of the heat rejection capability would be expected to occur following an aircraft crash. Thus, a crash into a spray pond would result in only a partial loss of the ultimate heat sink.

11.0 Aircraft Impact Upon the Switchyard

The term "switchyard" is used in a broad sense to refer not only to the actual switchyard but to include other electric transmission-related equipment as well. An aircraft crash into the switchyard could cause the loss of offsite alternating current power, depending upon the precise crash location. In such a case, it would be necessary to provide emergency AC power using the diesel generators. The crash and fire-induced damage in the switchyard could cause the propagation of electrical faults and result in the failure of electrical equipment in other areas upon the site. An example, a crash that not only results in the loss of normal AC power but also causes a massive short circuit that precludes the delivery of emergency AC power from the diesel generators would initiate an immediate loss of heat sink accident. A plane which crashes into the switchyard might tend to skid along the ground damaging the equipment within its path.

12.0 Aircraft Crash Upon a Diesel Generator Building

In the event of the loss of normal (offsite) alternating current power, the diesel generators provide emergency AC power.

12.1 VVER-440 Models V230 and V213

The diesel generators for both units of each plant are housed inside of the same building. The reference aircraft could readily penetrate through the roof or walls. The consequences are dependent upon the precise crash location. It is not known to what extent the individual diesel generators are separated by walls that could localize the effects of a crash and preclude the simultaneous loss of all of the diesels. In all cases, a fire could occur inside the diesel generator building. The diesel fuel provides an additional fire source. If sufficient structural and fire retardant barriers are not provided, then a crash could result in the loss of all of the diesel generators, depending upon the impact point. In the event of an extended loss of offsite AC power, the unavailability of emergency AC power would initiate a loss of heat sink accident. In particular, the feedwater and emergency feedwater pumps are electrically driven and require AC power for operation. For sites having more than one module (corresponding to more than two units), the diesels provided for the other units may constitute another source of emergency, onsite AC power.

12.2 VVER-1000 Model V320

Each VVER-1000 model V320 unit is provided with three diesel generators. The diesels for all of the units are distributed among several buildings constructed beside and between the reactor buildings of the various units. Each individual building houses three diesel generators, except for the first and last buildings which typically contain only one or two diesels. The reference aircraft could readily penetrate through the roof or walls of one of the diesel generator buildings. In all cases, a fire could take place inside the diesel generator building. The diesel fuel provides an additional fire source. Individual diesels are separated by reinforced concrete walls approximately 0.6 meter (2.0 feet) thick which could limit the effects of a crash to only one or two of the diesels. Due to the separation of the diesels among several structures, it is expected that for multiunit VVER-1000 model V320 stations, sufficient emergency AC power would likely be available following a crash into one of the diesel generator buildings. The VVER-1000 model V320 units are

constructed in series and it is possible that only one unit is on-line while the diesel generators corresponding to other units have not yet become operational. In such a situation, a crash which damages all of the completed diesels inside one of the diesel generator buildings could preclude the availability of adequate AC power, depending upon the minimum number of generators required. In the event of an extended loss of offsite AC power, the unavailability of sufficient emergency AC power could initiate a loss of heat sink event. For example, the condensate pumps, which draw water from the turbine condensers, are electrically driven and require AC power. However, the main feedwater pumps are driven by dedicated steam turbines, although their ability to continue operation in the absence of electrical power is uncertain.

13.0 Aircraft Impact Upon the Plant Ventilation Stack

13.1 VVER-440 Models V230 and V213

For each VVER-440 plant, a tall ventilation stack is located between the reactor building housing the two units and the radwaste building or beside the radwaste building on the side away from the reactor building. An exception is the Paks station in Hungary which has two stacks per two unit module that are located side-by-side and are not as tall as the stacks at the other VVER-440 stations. An aircraft crash into the stack could result in its collapsing or overturning upon one of the neighboring buildings. For specific VVER-440 plants, the stack location and stack dimensions are such that a portion of it could fall upon the reactor building. Depending upon the crash location, penetration of the roof of the reactor building above the reactor bay could occur. In particular, the stack could fall directly above one of the accident localization volumes. In this case, debris might impinge upon the protective shielding dome or cap above the reactor vessel of one of the units. Penetration of the dome or cap could result in damage to the control rod drive assembly above the vessel upper head. This might initiate a primary system loss-of-coolant accident in the upper head region. Control cables routed from the drive assembly may be damaged or severed. If steam or feedwater piping inside the reactor building is ruptured, a partial or total loss of heat sink accident could be initiated for one of the units. If debris impacts upon the refueling pool, spent fuel assemblies may be directly damaged by debris fragments resulting in the release of fission products. Damage to the refueling machine or the fuel handling equipment located between the two units could prevent refueling operations from being carried out until repairs have been made. If the stack falls upon the radwaste building, penetration of the radwaste building roof could occur. Damage to components of the primary system purification system for one of the units might preclude continued operation of that unit. If falling debris ruptures any of the storage tanks holding liquid wastes, then a release of liquid activity could occur. Depending upon the crash location, damage to components used to extract and concentrate wastes or the facilities provided for repair and maintenance of active components could occur. The inability to vent radioactive aerosols and gases at a suitable height above ground would require the two units to be shut down so as not to violate site release criteria. It is also possible for the upper portion of the stack to fall upon the central region of the turbine building. Damage to secondary components or piping could initiate a partial loss of heat sink accident.

13.2 VVER-1000 Model V320

Each VVER-1000 model V320 station incorporates a single tall ventilation stack located next to the common radwaste building serving all of the VVER-1000 model V320 units at the site. The station layout is such that the stack cannot fall upon any of the reactor buildings. However, depending upon the precise impact location, an aircraft crash could result in the collapse or overturning of the stack upon the radwaste building, neighboring waste processing buildings, or the elevated corridors joining the radwaste building with the reactor buildings. If the stack falls upon the radwaste building, damage to systems associated with wastes from the primary purification system could preclude the continued operation of all of the reactor units. If falling debris ruptures any of the storage tanks containing liquid wastes, then a release of liquid activity can occur. Depending upon the crash location, damage to components employed to extract and concentrate wastes or facilities provided for repair and maintenance of active components could occur. A portion of the stack could fall upon one of the elevated corridors containing pipes that transport radioactive wastes. Damage

of piping that carries wastes from the primary coolant purification system could preclude continued operation of one or more of the units. In any case, the inability to vent radioactive aerosols and gases at a suitable elevation above ground could require that all units be shut down so as not to be in violation of site release criteria.

14.0 Aircraft Impact Upon an Elevated Waste Corridor

14.1 VVER-440 Models V230 and V213

The reactor building and radwaste building of each VVER-440 plant are joined by an elevated corridor through which the transport of radioactive wastes takes place. The reference aircraft would readily penetrate through the corridor. If piping connected with the primary coolant purification system is ruptured, then continued operation of the two reactors could be precluded.

14.2 VVER-1000 Model V320

A network of elevated corridors, through which the transport of radioactive wastes takes place, joins the reactor building of each unit with the radwaste building and the radwaste building with the ventilation stack as well as neighboring waste processing facilities. The reference aircraft could readily penetrate through any of the corridors. The consequences are dependent upon the crash location. If the corridor exiting from the reactor building of only one unit is impacted, then the consequences will likely be limited to that unit. However, there are corridors which simultaneously serve several or all of the units. Damage of piping associated with wastes from the primary coolant purification system might preclude continued operation of the affected units.

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**Table 1 Potential Consequences of Aircraft Crashes at a VVER Site
Assuming Penetration of Structural Barriers**

Crash Location	Potential Consequences
Reactor Building	Immediate Breaching of Accident Localization Volume or Containment Boundary Primary System Loss-of-Coolant Secondary System Loss-of-Coolant Loss of Emergency Core Cooling Partial Loss of Heat Sink Total Loss of Heat Sink Core Degradation and Meltdown Damage to Reactor Shutdown and Control Systems Severing of Emergency Protection and Control Cabling Loss of Electrical Power Inside the Reactor Building Loss of Coolant from Refueling Pool Damage of Spent Fuel Assemblies and Release of Activity Destruction of Primary and Backup Control Rooms Damage to Refueling Machine and Fuel Handling Equipment
Turbine Hall	Secondary System Loss-of-Coolant Partial Loss of Heat Sink Total Loss of Heat Sink Loss of Condenser Cooling Water Partial Loss of Ultimate Heat Sink Total Loss of Ultimate Heat Sink
Pumping Station	Loss of Condenser Cooling Water Partial Loss of Ultimate Heat Sink Total Loss of Ultimate Heat Sink Loss of Intermediate Cooling System Water Loss of Service Water Loss of Electrical Power Inside the Pumping Station
Cooling Water Inlet Canal	Loss of Ultimate Heat Sink
Switchyard	Loss of Off-Site AC Power Fault Propagation-Induced Electrical Failures
Crash Location	Potential Consequences

Diesel Generator Building	Loss of Emergency AC Power Loss of Heat Sink
Radwaste and Workshop Building	Loss of Primary System Purification Release of Waste Activity Loss of Electrical Power Inside the Radwaste Building
Ventilation Stack	Collapse of Stack or Impact of Fragments Onto Reactor Building Collapse of Stack or Impact of Fragments Onto Turbine Building Collapse of Stack or Impact of Fragments Onto Radwaste Building Collapse of Stack or Impact of Fragments Onto Elevated Waste Corridors
Elevated Waste Corridor	Loss of Primary System Purification
Cooling Tower	Partial Loss of Ultimate Heat Sink
Spray Pond	Partial Loss of Ultimate Heat Sink

Table 2 Characteristics of Selected Soviet Military Aircraft

Aircraft	NATO Code Designation	Gross Weight	
		kilograms	(pounds)
Mig-21	Fishbed	9,500	(21,000)
Mig-23	Flogger	16,000	(35,000)
Mig-25	Foxbat	29,100	(64,200)
Mig-27	Flogger D/J	17,000	(37,000)
Mig-29	Fulcrum	27,000	(60,000)
Mig-31	Foxhound	29,100	(64,200)
Su-7	Fitter A	13,000	(29,000)
Su-11	Fishpot	14,000	(30,000)
Su-15	Flagon	16,000	(35,000)
Su-20	Fitter C/D	18,000	(39,000)
Su-24	Fencer	29,500	(65,000)
Su-27	Flanker	28,800	(63,500)
Tu-28	Fiddler	37,000	(82,000)
Yak-28	Firebar	14,000	(30,000)
Yak-38	Forger	12,000	(26,000)

ANALYSIS APPENDIX K
BORIC ACID INDUCED CORROSION

TABLES

1. Selected Test Rig Corrosion Rate Data

FIGURES

1. Reactor Coolant Stud Bolt from a PWR. The original diameter was 3 1/2 inch; minimum remaining diameter, 1.1 inch⁽¹⁾
2. Areas on the Reactor Vessel Head of Turkey Point 4 Affected by Boric Acid⁽⁴⁾
3. (a) Typical Configuration of High-Pressure Injection Nozzle at ANO-1
(b) Areas of Boric Acid Corrosion Damage⁽⁴⁾
4. Measured Corrosion Rates of Low Alloy Steels (inch/year) as a Function of Temperature⁽¹⁾

ANALYSIS APPENDIX K

BORIC ACID INDUCED CORROSION

1.0 Introduction

The primary coolant of all PWR and VVER units contains dissolved boron up to 2500 ppm boron (as boric acid). The boron functions as a neutron absorber for reactivity control. In addition there are variable concentrations of LiOH (most PWRs) or KOH (VVERs) to raise the pH of the coolant to minimize corrosion and CRUD transport, and hydrogen (most PWRs) or ammonia (VVERs) to maintain the oxygen concentration below 0.1 ppm to minimize corrosion. When the coolant is maintained within the specified limits, the resistance to general corrosion and stress corrosion cracking of pressure-containing components of both PWRs and VVERs has been excellent. With careful attention to pH and oxygen, VVERs with unclad vessels have been operated with minimal corrosion and CRUD transport problems.

Problems have developed when the primary coolant has leaked at operating temperature and pressure, and the water flashed into steam, leaving behind local deposits of a wet sludge of boric acid crystals. These deposits, even when LiOH and KOH are present, are acidic at approximately 100 degrees Centigrade. This acidity, combined with ready access to oxygen, has produced locally high corrosion rates of ferritic steel components, most notably in low alloy steel bolts and, where it has dripped on reactor vessel heads, the vessel heads themselves. In the VVER-440 model V-230, with unclad vessels, leaking coolant would be highly corrosive to the pressure vessel steel flanges as well as the bolts.

This appendix will review the experience with corrosion by leaking primary coolant, the laboratory data that simulate the effect, and discuss the potential consequences and possible remedial actions for the problem.

2.0 Experience with Corrosion by Leaking Primary Coolant

Experience in the United States with corrosion resulting from leaking primary coolant has been reviewed by J. F. Hall et al., of Combustion Engineering, under an EPRI project^[2,3,4]. Other workers have also examined the degradation of bolts in PWRs. All agree that the corrosion rates are high. The following observations have been made.

- Since 1968, there have been at least thirty-two instances of severely corroded bolts in PWRs attributable to leaking boric acid. Figure 1^[2] shows a reactor coolant pump closure stud removed from the Fort Calhoun PWR showing degradation - the original diameter of the bolt was 3 1/2 inches. The material was ASTM A193-B7, a low alloy steel. In this instance, six of sixteen studs on one pump and three of sixteen studs on a second pump had been significantly corroded.
- At Calvert Cliffs, corrosion was also observed on the suction piping (ferritic steel, clad internally with stainless steel) to the primary coolant pumps. The corrosion (approximately 1/8 inch grooving) was attributed to leaks in pump seal/bleed-off lines travelling down the pump casings inside (or through) the insulation. Corrosion on pump stud bolts was also observed, attributable to boric acid leaking through gaskets.

- At Turkey Point-4 ^[6], more than 500 pounds of boric acid crystals were discovered on the reactor pressure vessel head in March 1987. After removal, severe corrosion of several areas was noted, as sketched in figure 2. Most of the damage was believed to have occurred in approximately a six month period. Wastage around the closure head studs did not exceed 1/16 inch in depth. A three foot section of the vent shroud support ring was consumed by the boric acid. The leak, 0.25 gallons/minute, was in a conoseal assembly where the instrumentation cable enters the reactor vessel, and was within technical specification limits.
- At Arkansas Nuclear One - Unit One, a leak in a high pressure injection (HPI) manual isolation valve, occurring over a six-month period at a rate of less than 0.1 gpm, caused a maximum corrosion depth of 0.5 inches on the HPI nozzle and adjacent primary coolant system cold leg piping (both carbon steel). The affected areas are sketched in figure 3. The maximum corrosion depth (0.5 inch) occurred at the nozzle's minimum wall thickness (0.75 inch including an interior stainless clad).
- A fifty percent reduction of two or three adjacent studs has been observed in a French PWR.
- Reactor pressure vessel head penetrations in VVERs in some CMEA countries are inspected monthly for the build up of boric acid crystals because of concern over boric acid wastage, after leakage from a nuclear instrument tube penetration in the head was discovered at Paks Unit 2.
- Each operating U.S. PWR is required to have a program in place to address the corrosive effects of reactor coolant leakage at less than technical specification limits.^[5]

3.0 Corrosion Mechanisms and Laboratory Corrosion Results

The corrosion occurring is believed to be an acid wastage type process, accelerated by oxygen, although the detailed mechanism is not well understood. When the primary coolant leaks out at operating temperatures, most of the water flashes into steam, leaving behind a moist sludge of crystals of boric acid along with any LiOH or KOH added to control the pH of the primary coolant. The temperature of the sludge is kept to approximately 235 degrees Fahrenheit by boiling. Since boric acid is weakly-ionized in the reactor coolant, the presence of only a few ppm of KOH or LiOH is required to maintain the desired pH. In the sludge, however, these alkalis have little effect, since they represent < 1 percent of the amount needed to neutralize all the boric acid. Since the corrosion is an acid wastage, the rate of attack is a function of local flow rate in the sludge, as well as the local availability of oxygen.

A number of attempts have been made to simulate this in the laboratory. Figure 4 shows some data of Czajkowski's ^[1] which clearly indicate the temperature-insensitivity above 220 degrees Fahrenheit and the lack of any effect of LiOH or KOH additions above this temperature. In these experiments, the maximum wastage rates were approximately 0.13 inches per year. Other laboratory data give higher rates. Westinghouse obtained data on carbon steel (cited in references 3 and 5) as high as 0.4 inches per month. Hall^[2] gives the results shown in Table 1, where it can be seen that average corrosion rates for some bolting materials exceeded 0.7 inches per year, with maximum rates up to twice that level. Hall's data also clearly show that type 304 stainless steel, 17-4 pH stainless steel, and alloy 718 are resistant to this type of attack. Other laboratory data, reported by Hall ^[4], show 220 degrees Fahrenheit corrosion rates of pressure vessel steels are high for the first few hours (> 1 inch/year) and decrease over the next few days to approximately 0.25 inches/year. This observation, combined with a broad range of possible conditions at leakage sites in reactors makes extrapolation to field conditions difficult. The wastage shown in figure 1 occurred at a rate no less than 0.2 inches/year.

4.0 Consequences and Remedial Actions

Leaking coolants that can corrode critical bolting materials, Reactor Pressure Vessel heads, or primary piping at rates up to 1 inch/year (or even higher) clearly represent a risk that compromises the integrity of the reactor coolant pressure boundary. The buildup of boric acid crystals, however, is frequently visible and can provide an early warning of developing problems, if inspections are made at frequent enough intervals and remedial actions taken, and the deposits not obscured by insulating materials.

As noted above, some CMEA countries inspect the reactor vessel head penetrations monthly.

The French have the following general requirements; 1) make leaks an exception; 2) be prepared for leaks (use more corrosion resistant (i.e., higher Cr stud materials) and 3) inspect for leaks.

5.0 Relevance to VVERs

The pressure vessel steels (and bolting steels) in all VVERs would be susceptible to the type of wastage corrosion by leaking primary coolant, described above. The KOH added to the coolant for pH control has been shown to have no effect on this type attack. Leaks at reactor vessel head penetrations would be especially a concern, and some CMEA countries reportedly inspect for these monthly.

- VVER-440 model V-230: The use of unclad pressure vessels in these units implies that the pressure vessel head flanges are also unclad, so that any leaking boric acid would also tend to corrode the flange surfaces as well as the stud bolts, which could lead to accelerated leakage. The use of stainless steel piping eliminates concern from boric acid accumulating on piping surfaces.
- VVER-440 model V-213: The stainless steel cladding on the flange surface would resist attack by leaking boric acid. Otherwise, the situation is similar to that in model V-230s.
- VVER-1000: The use of low alloy steel piping clad with stainless steel on the inside makes the situation with these units quite similar to that in Western PWRs, in that the outer surfaces of both the pressure vessel and the piping are low alloy steels and susceptible to corrosion where leaking primary coolant allows boric acid deposits to form.

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Table 1 Selected Test Rig Corrosion Rate Data [2]

Material	Corrosion Rate, Inches/Year			
	175 Degrees Fahrenheit		350 Degrees Fahrenheit	
	Max	Ave	Max	Ave
SA-193B7 [1]	.83	.64	.43	.19
SA-540B24 [1] Class 4	.82	.73	.84	.65
SA-540B24 [1] Class 3	.26	.26	1.3	.50
17-4 pH [2]	0	0	0	0
Alloy 718 [2]	0	0	0	0
Type 304SS [2] (Drip Test)	0	0		

[1] Low Alloy High Strength Bolting Steels Containing up to 1.5 Percent Cr and up to 0.3 Percent Mo.

[2] Stainless Alloy Containing 17 Percent or More Cr.

Figure 1 Reactor Coolant Stud Bolt from a PWR. The Original Diameter was 3 ½ Inch; Minimum Remaining Diameter, 1.1 Inch ⁽¹⁾

Figure 2 Areas on the Reactor Vessel Head of Turkey Point 4 Affected by Boric Acid ⁽⁴⁾

AREA OF CORROSION DAMAGE

Figure 3 (a) Typical Configuration of High-Pressure Injection Nozzle at ANO-1;
(b) Areas of Boric Acid Corrosion Damage ⁽⁴⁾

Figure 4 Measured Corrosion Rates of Low Alloy Steels
(inch/year) As A Function of Temperature ⁽¹⁾