IAEA-TECDOC-593

Case study on the use of PSA methods: Station blackout risk at Millstone Unit 3

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CASE STUDY ON THE USE OF PSA METHODS:
STATION BLACKOUT RISK AT MILLSTONE UNIT 3
IAEA, VIENNA, 1991
IAEA-TECDOC-593
ISSN 1011-4289

Printed by the IAEA in Austria
April 1991

FOREWORD

Probabilistic Safety Assessment (PSA) is increasingly being used to complement the deterministic approach to nuclear safety. From the traditional discipline of reliability engineering, PSA developed as a structured method to identify potential accident sequences from a broad range of initiating events and to quantify their frequency of occurrence.

PSAs use inductive (event tree) and deductive (fault tree) logic and plant specific as well as generic component failure rates and frequencies of initiating events. Plant specific test and maintenance schedules, human errors and common cause failures are also considered in the probabilistic models.

PSA is nowadays a fundamental tool that provides guidance to safety related decision-making. By its very nature PSA recognizes the uncertainties associated with the logic models used to represent reality and quantifies the variability in the data of the parameters in the models.

The IAEA is promoting the conduct of PSA studies through standardization of the methodology, co-ordination of research, assistance through its Technical Co-operation Programme, and development of PSA software (PSAPACK). In addition it offers International Peer Review Services (IPERS) to review PSAs at various stages of completeness.

Emphasis at present is concentrated on "level-1" PSAs which quantify accident sequences up to estimates of core-damage probability. Level-2 (releases of radioactivity) and level-3 (off-site impacts) will be addressed at a later stage.

The work described above on the conduct of PSA is complemented by a programme on how to use the results of PSA in nuclear safety. For this purpose a series of CASE STUDIES has been prepared. The objective is to provide those who have performed PSAs with practical examples on how PSA results have been used. Those authorities and utilities still reluctant to request or perform PSAs will find convincing evidence on the benefits of such studies for nuclear safety.

With these objectives in mind, the IAEA requested a number of internationally recognized experts to document, in a uniform and suitable format, actual experience with the use of PSA for safety decisions. The documents were peer reviewed by an Oversight Committee for quality and completeness.

It is hoped that this series of CASE STUDIES will significantly contribute to the use of PSA to improve nuclear safety.

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The information contained in this Case Study has been prepared for the specific requirements of Argonne National Laboratory under Contract No. 62202401. The information will be made available to the IAEA and others without consideration in the interest of promoting the spread of technical knowledge. Any use of all or any portion of the information, analyses, methodology or data contained in the subject Case Study by third parties shall be undertaken at such party's sole risk.

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PREFACE

A series of CASE STUDIES has been prepared to summarize practical examples on how the results of PSA studies have been used in nuclear safety. They draw from the experience of major studies and, to the extent possible, use a similar format to guide the reader. The studies illustrate the range of applications in a specific topical area. It is the objective to take examples which are using level-1 PSAs rather than individual accident sequences or systems reliability. Emphasis is given to a logical step-by-step description of the analysis and documentation of calculational procedures and data. The interpretation of the results explicitly addresses the problem of uncertainties and limitations of the studies, and includes the results of Peer Reviews.

This CASE STUDY addresses the problem of station blackout using the example of the Millstone Unit 3 Pressurized Water Reactor. Many PSAs have identified the importance of this initiating event potentially leading to core-melt accidents. In order to identify further improvements an accurate representation of different types of core-melt scenarios involving specific areas of vulnerability had to be attained. Therefore, it was necessary to use time dependent PSA methods to provide a more realistic treatment of time dependent failure and recovery.

The purpose of this CASE STUDY is thus to provide a good example on how the critical parameters for decisions regarding backfitting to cope with station blackout can be identified and quantified using PSA techniques.

The following additional Case Study documents are available:

IAEA-TECDOC-522	A Probabilistic Safety Assessment Peer Review: Case Study on the Use of Probabilistic Safety Assessment for Safety Decisions (1989)
IAEA-TECDOC-543	Procedures for Conducting Independent Peer Reviews of Probabilistic Safety Assessment (1990)
IAEA-TECDOC-547	The Use of Probabilistic Safety Assessment in the Relicensing of Nuclear Power Plants for Extended Lifetimes (1990)
IAEA-TECDOC-590	Case Study on the Use of PSA Methods: Determining Safety Importance of Systems and Components at Nuclear Power Plants (1991)
IAEA-TECDOC-591	Case Study on the Use of PSA Methods: Backfitting Decisions (1991)
IAEA-TECDOC-592	Case Study on the Use of PSA Methods: Human Reliability Analysis (1991)

CONTENTS

1. PROBLEM DEFINITION

In Westinghouse pressurized water reactors (PWRs), severe accident sequences resulting from Station Blackout have been recognized to be significant contributors to risk of core damage and public consequences. The Station Blackout accident scenario involves a loss of offsite power, failure of the redundant emergency diesel generators, successful operation of the steam driven auxiliary feedwater (AFW), failure of AC power restoration and the eventual degradation of the reactor coolant pump (RCP) seals resulting in a long term loss of coolant. If AC power is not recovered (either onsite or offsite) it is not possible to provide makeup to the reactor to compensate for the loss of coolant through the RCP seals. Over the long term period this could result in an eventual core damage, the potential for containment failure, and significant consequences to the public.

RCP seal degradation, while recently given considerable attention as possibly the dominant mechanism to reach a degraded core state following Station Blackout, is by no means the only mechanism. If the seals retain their integrity long enough other effects become equally important to consider in quantifying risk. These effects include:

- Complete discharge of the station batteries (DC Blackout) resulting in the inability to control equipment if AC power is not restored and a complete loss of all vital instrumentation.

- Loss of all instrumentation power due to common cause failures in the inverters caused by the inevitable loss of room cooling following the Station Blackout. (Given constant heat rejection by the inverters and no heat removal in the switchgear room, failure is inevitable at some point.)

- Loss of the steam driven AFW pump due to the high temperatures resulting from the consequential loss of all HVAC in the AFW pump compartment.

To properly quantify the risk of Station Blackout, it is thus necessary to consider all of these possible types of core damagescenarios. Having obtained an accurate representation of the types of core damage scenarios involved specific areas of vulnerability can be pinpointed for further improvement.

2. OBJECTIVES

Earlier analysis of Station Blackout events in PWRs using Westinghouse RCPs had identified RCP seal failure as a dominant issue. Major modifications had been proposed such as steam driven RCS makeup pumps or complete redesign of the RCP sealing system. The Station Blackout investigation for Millstone Unit 3 was performed to gain a better perspective of the plant specific risk from Station Blackout events and what features of the plant or aspects of operation most contribute to that risk. Among the areas to be investigated the following were selected as a result of screening analysis which considered the conditions and limitations they would impose on plant safety:

- The reliability of the offsite power grid and the influence of severe storms such as Hurricanes on grid reliability. (This is characterized by the mean frequency of loss of offsite power.)

- The ability to restore offsite power supply to the station loads. (This is characterized via mean restoration time.)

- The reliability of the RCP sealing system to maintain integrity under prolonged loss of thermal barrier cooling and seal injection caused by the Station Blackout. (This is characterized by a coping time which if exceeded results in core damage.)

- The capacity of the Station Batteries. (This is characterized by a discharge time which if exceeded will result in core damage due to the inability to monitor and control core cooling by natural circulation.)

- The reliability of the 120V Vital AC power system (supplied by inverters from the Station Batteries) to supply critical instrumentation needed to monitor core cooling. The inverters must function in the switchgear room for a prolonged period of time without room cooling.

- The reliability of the steam driven AFW pump to function for extended periods of time without room cooling.

Items such as consequential loss of instrument air, and control room cooling were evaluated via preliminary screening analysis and it was concluded that their failures did not effect core damage risk or were so delayed that other issues would be controling. As an example: loss of instrument air would have no impact because following the onset of Station Blackout little equipment would be left that was dependent on instrument air. The one exception: the steam inlet valves to the AFW are designed to fail open on loss of air. Hence, the consequential loss of instrument air would not aggravate the Station Blackout.

In performing this analysis it was decided to use time dependent PSA methods to provide a more realistic treatment of time dependent failure and recovery. This is because considerable periods of time are involved in the core degradation process and the likelihood of restoring power increases with passing time.To have treated the problem using standard time-averaged unavailability calculations would have resulted in an unduly conservative perspective that might result in focusing attention on the wrong areas for improvement.

3. OVERVIEW OF THE ANALYSIS

As an overview of how the analysis was carried out: the PSA quantification is basically a calculation of a Station Blackout event (a particular failure) occurring and not being restored for a time period longer than the plant's capability to cope with the event.

The processes leading to core damage in this type of sequence are represented as sequence 7 in the simplified functional event tree shown in Figure 1. A loss of offsite power is the initiating event. If the emergency generators operate such that at least one train is

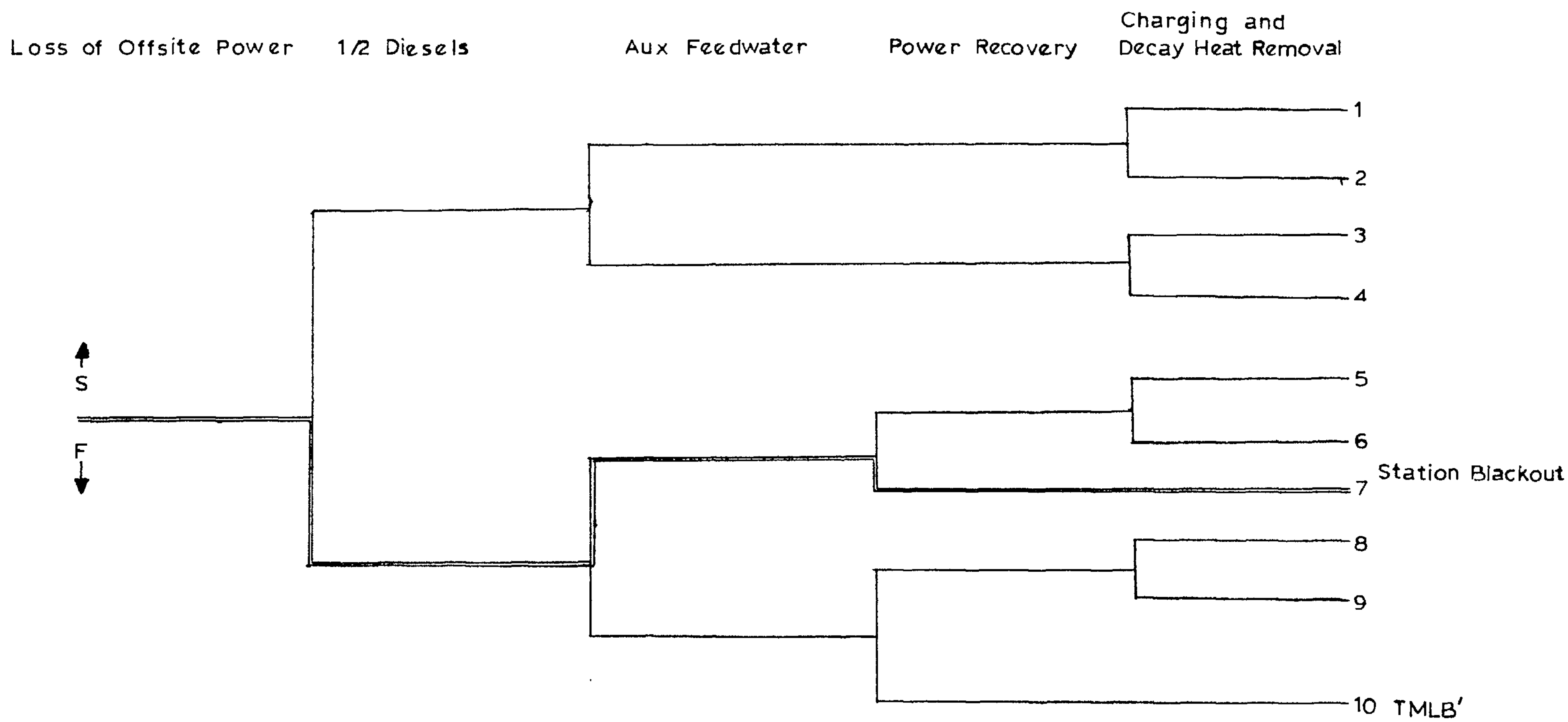


FIG. 1. Station blackout sequence.

available to power decay heat removal systems all that is necessary is for AFW to supply cooling water for the steam generators and the charging system to supply seal injection flow to the reactor coolant pumps and RCS makeup flow to compensate for shrinkage. Given the availability of power the failure of AFW or charging system is very unlikely. If the emergency generators are both unavailable and the steam driven AFW pump starts and operates to remove decay heat, core damage is not likely in the short term. If power is recovered before RCP seals degrade or the batteries become discharged a normal shutdown can be commenced. If power recovery takes longer than the plant coping time, core damage will result. The recovery time is sequence specific and is highly dependent on the availability of AFW. The recovery time for sequence 10 (generally classified as a TMLB' sequence) is much shorter because absence of steam generator cooling will result in long term operation of the pressurizer PORVs to control RCS pressure.

To quantify sequence 7, it is necessary to develop a plant specific coping time. The coping time is defined as the time period that the plant can withstand prolonged unavailability of onsite and offsite power without experiencing severe core damage. The coping time is recognized to be a random variable and is dependent on the following random effects which must each be considered:

- the rate and magnitude of RCP seal degradation
- the rate of core uncovery vs. the magnitude of RCP seal degradation
- the beneficial aspects of manual RCS depressurization by plant operators (this slows the leak rate and prolongs the time to core uncovery)
- the capacity of the station batteries
- the heatup characteristics of the switchgear and AFW pump compartments due to loss of all room cooling.

Figure 2 shows a simplified flow chart showing how a plant coping time distribution is developed and the type of information needed as inputs. Figure 2A represents, in the form of a time dependent event tree, how a convolution integral is used to obtain the time dependent probability calculation.

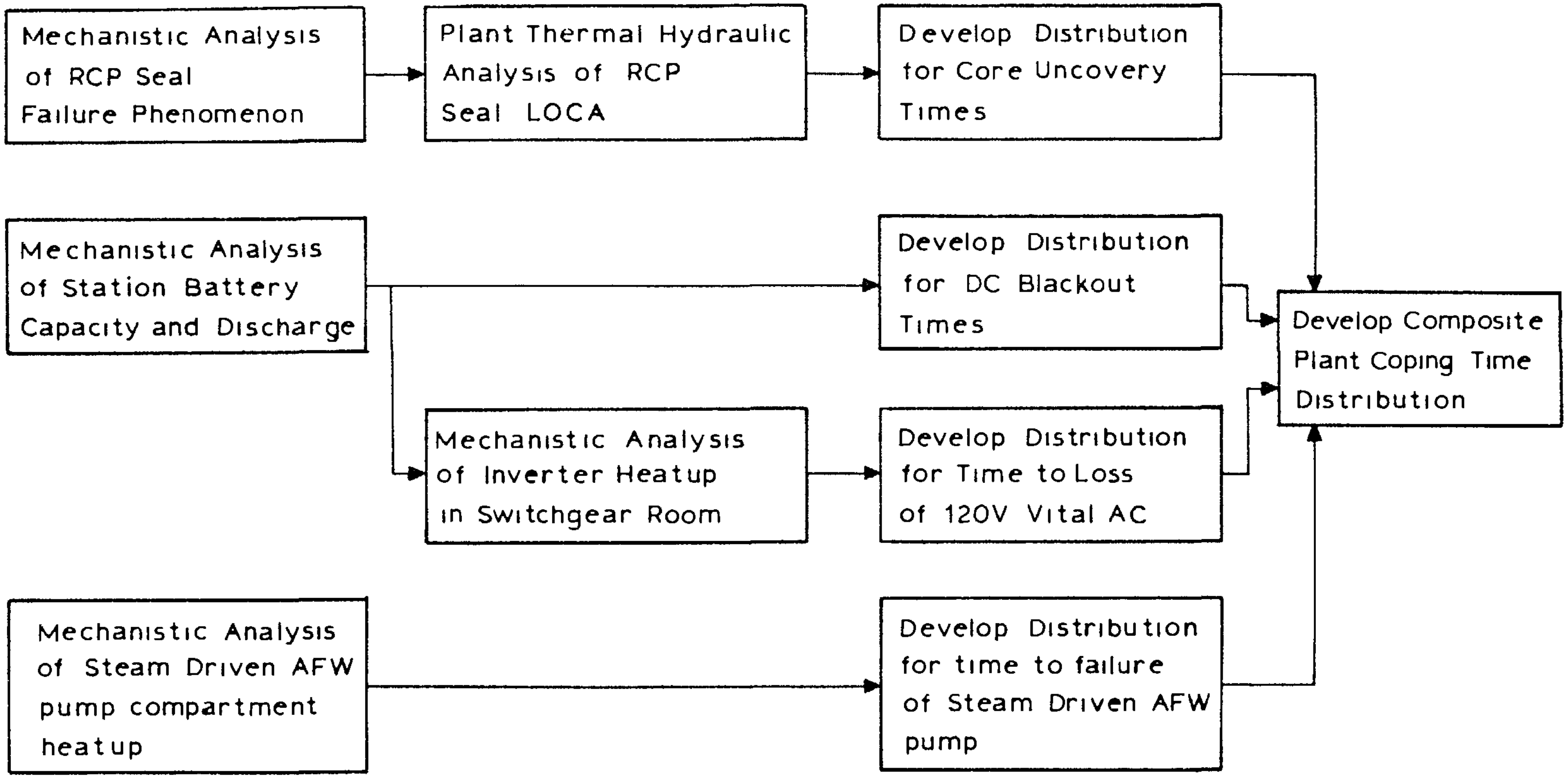
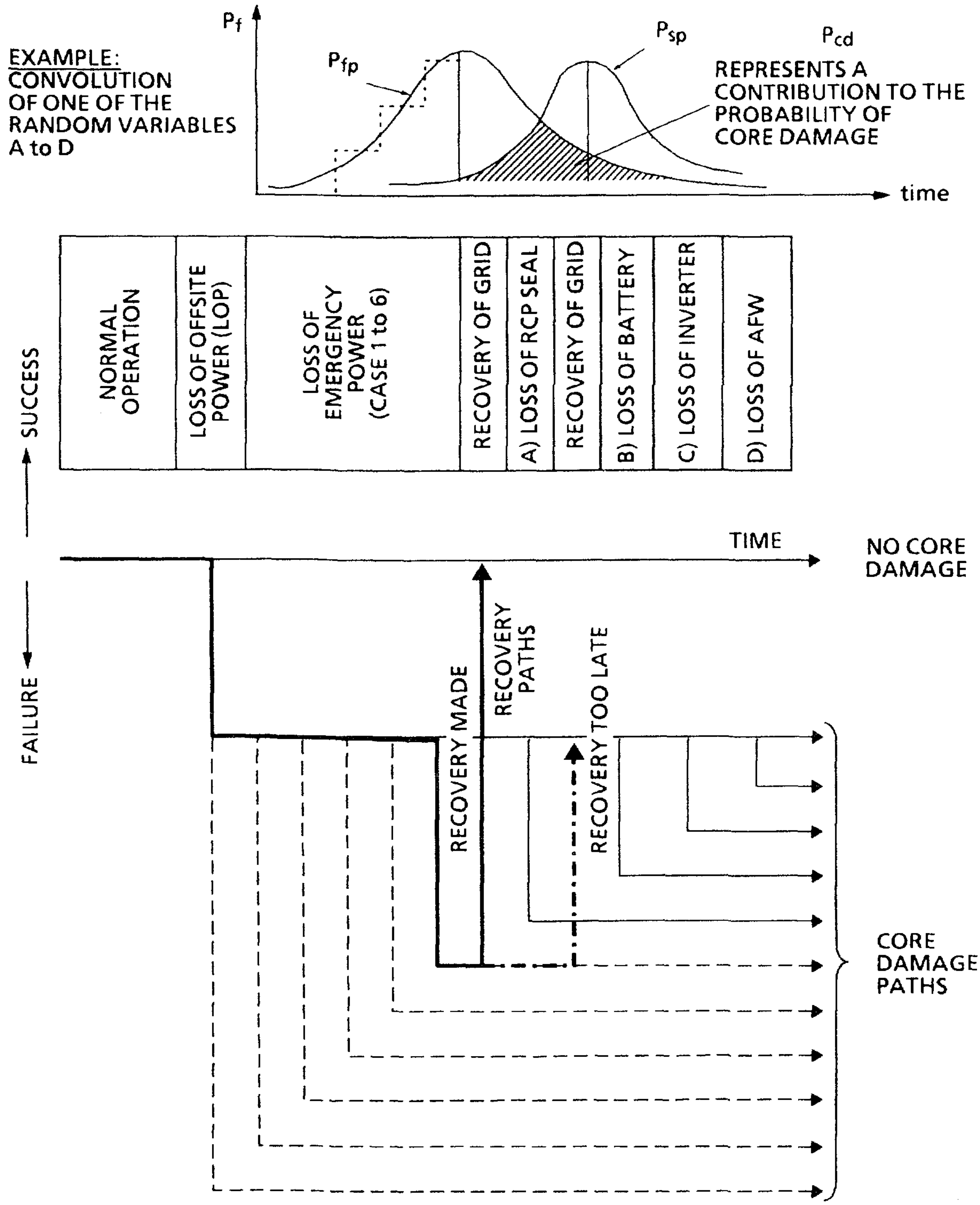


FIG. 2. Flow chart for development of plant coping time distribution.



Case 1-6: The different loss of emergency power cases;
P_{fp}: Probability density function of outage times (failure-paths) (a fit to step functions);
P_{sp}: Probability density function of recovery times (success-paths) (a fit to step functions);
P_{cd}: All cases for which the outage times are longer than recovery times and therefore it represents the total time dependent core damage frequency.

FIG. 2A. Schematic representation of station blackout in the form of a time dependent event tree.

The first area investigated is the behavior of the RCP seal system. In this area, the results from existing mechanistic analysis of RCP seals is utilized to determine the time dependent degradation rates of the seal material. This investigation yields information on the timing and leakage rates of the RCP seals. Following this, plant specific thermal hydraulic analysis is performed which bounds the various possible RCP seal leak rates and operator mitigation strategies ranging from: taking no action at all, to aggressively cooling down the RCS to the limit of just avoiding Accumulator N_2 injection (which could dramatically hinder natural circulation in the steam generators). From these analyses, a distribution of core uncovery times is developed vs. leakage rates. Using an event tree model whose end states represent different leak rates and whose nodes represent various RCP seal failure mechanisms, the probability of different leak rates can be developed. Convoluting the distributions of leak rate vs. probability and core uncovery times vs. leak rate, an overall distribution of core uncovery times vs probability can be generated.

The investigation of Station Battery capacity was carried out to determine what minimum voltages (stored battery charge) are necessary in order to restart key components in the AC power system. The following types of loads were considered:

- o emergency diesel starting loads including multiple start attempts, power control logic, generator field flashing, and breaker reclosing.
- o onsite AC power system breaker control logic, and breaker closing loads.
- o continuous loads such as powering inverters to supply 120V Vital AC which in turn provides power to all control room instrumentation necessary to maintain natural circulation cooling.
- o short term loads such as powering emergency turbine lube oil pumps.

Based on an analysis of discharge times and engineering judgement the probability distribution of battery discharge times was developed.

The investigation of Inverter heatup was performed to determine if the loss of room cooling due to the blackout conditions would result in temperatures high enough to disable all control room instrumentation. A similar investigation was performed to assess if steam driven AFW pump compartment heatup would result in pump failure.

For items judged as significant limitations to plant coping time, the distributions were combined using discrete probability distribution (DPD) arithmetic to yield a composite plant coping time.

Having developed a distribution function describing coping times, the probability of offsite and onsite power failing and not being restored for time periods greater than the coping time is computed as shown in Figure 3. The first step involves construction of a Station Blackout fault tree model. The cutsets from this fault tree model are quantified using time dependent unavailability expressions and data arising from plant specific and industry data sources. Further detail on the theory and bases of the mathematical expressions is provided in the following section.

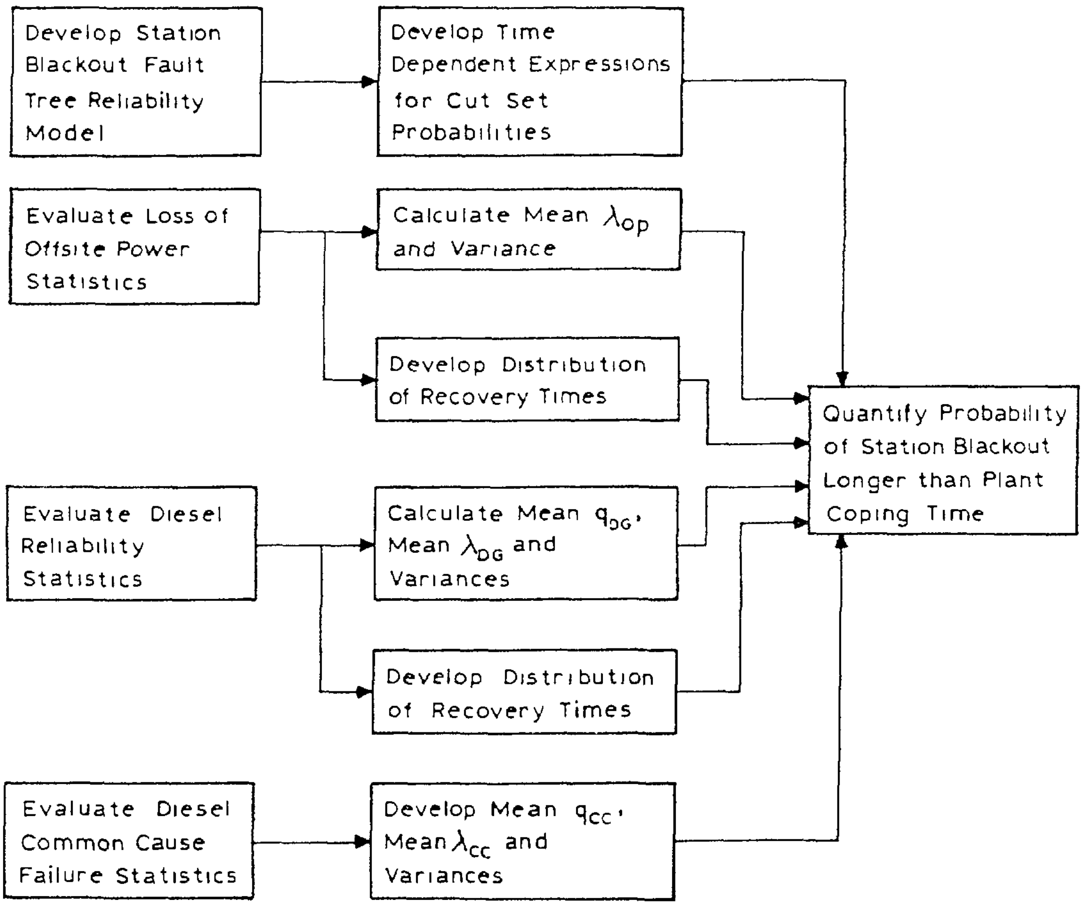


FIG. 3. Flow chart for quantification of station blackout longer than coping time.

4. CALCULATIONAL PROCEDURES AND METHODS

This section summarizes the overall analysis of Station Blackout core damage frequency including calculations related to plant coping time and the probability of a Station Blackout event lasting longer than the coping time.

4.1. Coping Time Evaluation and Related Considerations

4.1.1. Coping Time Due to RCP Seal Failure and Core Uncovery

RCP Seal Failure as a Function of Time

Earlier analysis (Reference 1) typically made the modeling assumption that given a 30 minute interruption in RCP Seal Cooling (with RCS temperatures greater than 400^{o}F), a catestrophic type RCP seal blowout would occur. These assumptions are equivalent to stating that, given a 30 minute interruption in RCP seal cooling with temperatures greater than 400^{o}F, the probability of a catastrophic seal failure would be: $p_f = 1.0$.

Reference 2 identifies 6 incidents in which operating nuclear power plant RCP seals were subjected to prolonged loss of cooling 30 minutes or longer at temperatures greater than 400^{o}F. (This experience data base did not include the results of controlled experimental tests which have been run for periods of as long as 20 hours without catastrophic failure.)

As a result of recent work (Reference 3) sponsored by the Westinghouse Owner's Group, considerable new information exists regarding O-ring performance. It has been recognized that there are really two issues affecting seal integrity under prolonged loss of cooling incidents:

- o Early failure (possibly in the 30 minute time frame) due to improper seating of the #1 RCP seals. The probability of such a failure mode is very difficult to calculate and involves conditions in which the seal ring binds on the pump shaft and remains in a full open position despite a considerable force balance which would tend to maintain the seals in a proper orientation (Reference 5).

- o Longer term leakage as a result of thermal and mechanical phenomena which may alter the leakage path profiles for RCS leakage.

RCP Seal Leak Rates

With the current RCP seals in place at Millstone Unit 3 Reference 3 would indicate that the nominal leakage is expected to be 21 gpm or less for the first two hours. Should subsequent failures of the secondary sealing O-rings and channel seals occur well into the event, the leakage rate could be as high as 76 gpm to 182 gpm per RCP.

A number of earlier probabilistic safety studies were performed making an assumption of a 300 - 500 gpm/RCP leak flow following failure of the RCP seals. This assumption is based on simplified calculations with critical flow at full system pressure (2250 psia) and enthalpy (550 BTU/lbm) for the minimum, cold condition, nominal clearances of fully opened seals. As it turned out, the high temperature conditions result in mechanical loadings which change the tolerances involved for fully opened seals. In that case, calculations estimate the leakage to be 480 gpm/RCP.

Obviously this assumption is excessively conservative, but such an assumption was typically made due to the lack of available test data on RCP seal performance under Station AC Blackout conditions. Because of the significance of this assumption on plant risk quantification, the Westinghouse Owner's Group sponsored an investigation of the response of the RCP seal system via a program of thermal hydraulic/analysis, component testing, and full scale RCP seal system testing.

Detailed thermal stress and thermal/hydraulic analyses were performed using mildly conservative assumptions for both the 8" standard and 8" cartridge seal assemblies subjected to the loss of all seal cooling. The results of the analysis indicated that the expected RCP seal leakage during a Station AC Blackout would be ~21 gpm/RCP provided the O-rings and channel seals do not fail.

The Westinghouse Owner's Group also participated in the full scale testing of a 7" RCP seal system under the conditions representative of

Station AC Blackout. This test was conducted at the Electricite de France (EdF) seal test facility in Montereau, France. The test results indicated a 20% lower flow rate than predicted by current analysis. Design evaluations completed by Westinghouse have indicated that the 7" RCP seal system which was tested is similar in design to the 8" RCP seal system which was analyzed.

Secondary Depressurization

Depressurizing the RCS using the steam generators will reduce the differential pressure across the seals thus reducing the RCP seal leak rates. This prolongs the time before onset of core uncovery. Earlier analysis (Reference 1) assumed no operator actions until 2 hours into the event. A 30 minute assumption on operator action is more realistic and consistent with current plant Emergency Operating Procedures (EOPs). These EOPs require the operator to initiate steam generator depressurization down to 260 psig via manually dumping steam at the maximum rate. This procedure would be entered immediately after normal post trip actions and attempts to restart the diesels.

Core Uncovery Time

Earlier analysis of core uncovery time was predicated on a 300 gpm/RCP leakage rate and without taking credit for secondary depressurization nor depressurization due to leak flow through the failed RCP seals. Plant specific best-estimate thermal hydraulic analysis was performed for Millstone Unit 3 by Westinghouse (the NSSS supplier) using the LOFTRAN Code. A spectrum of initial RCP seal leak rates between 50 gpm and 300 gpm were studied via best-estimate type analysis, with and without the effects of secondary depressurization. The results indicated that at least two hours would be available before the onset of core uncovery even if a 300 gpm/pump leak rate was assumed. Figure 4 shows the actual reactor vessel water level as a function of time assuming no cooldown. If the secondary plant is depressurized after one hour, the time until the onset of core uncovery is increased out to three hours as shown in Figure 5. Figures 6 and 7 respectively show the RCS pressure and temperature response to secondary depressurization. Figure 8 shows the predicted times to core uncovery given various leak rates at Millstone Unit 3 both with and without cooldown at 100^{O}F/hr.

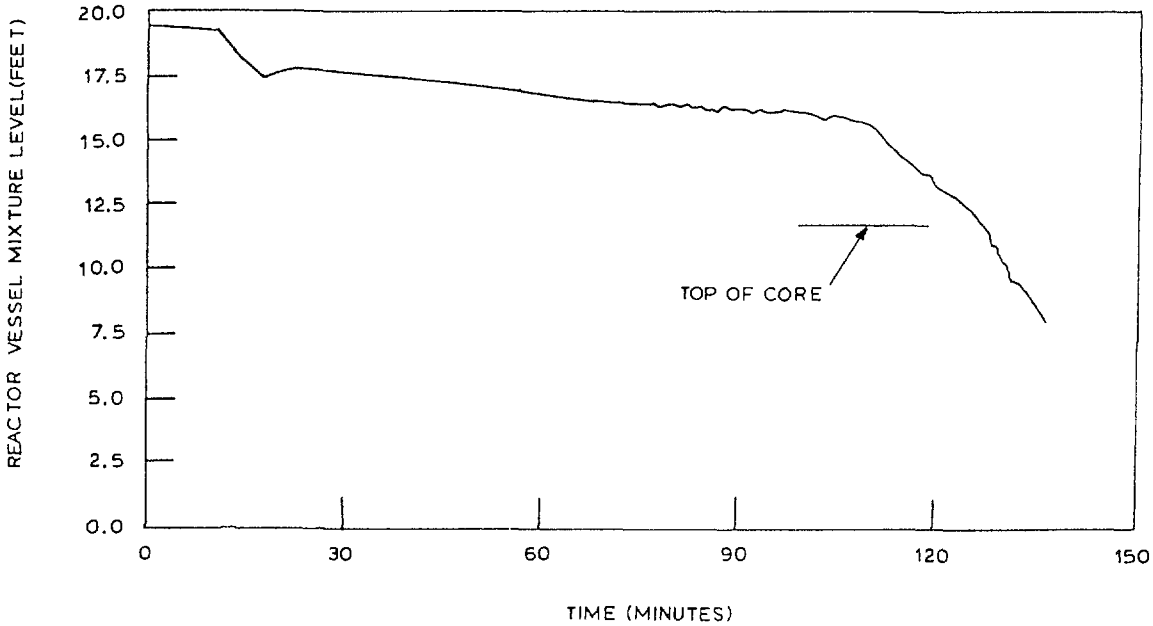


FIG. 4. Reactor vessel mixture level — 300 gpm/RCP leakage without secondary depressurization.

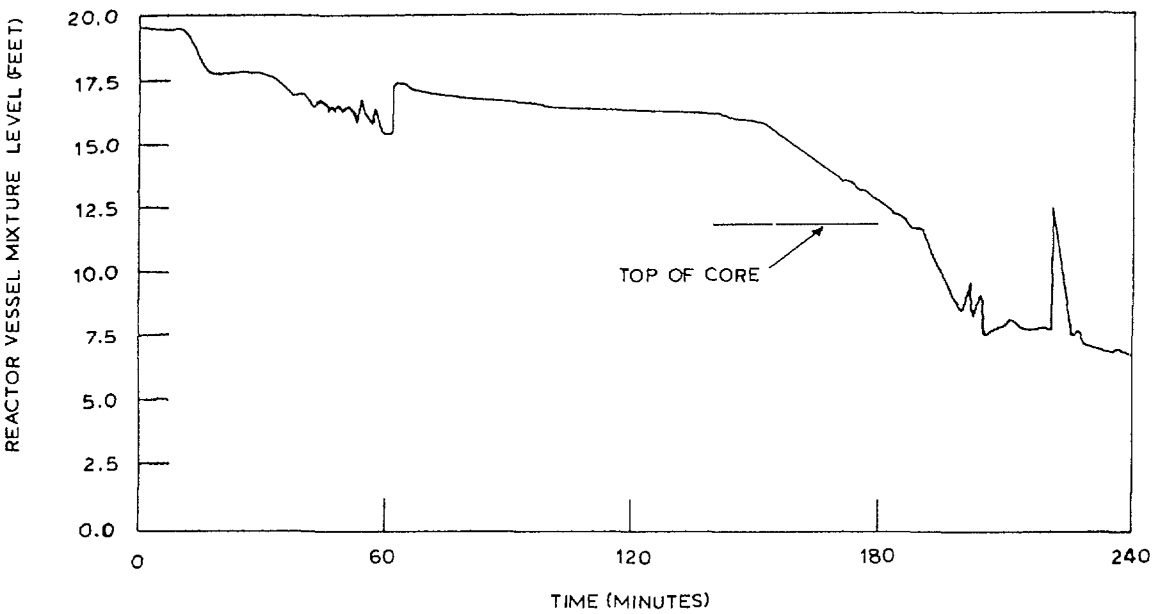


FIG. 5. Reactor vessel mixture level — 300 gpm/RCP leakage without secondary depressurization.

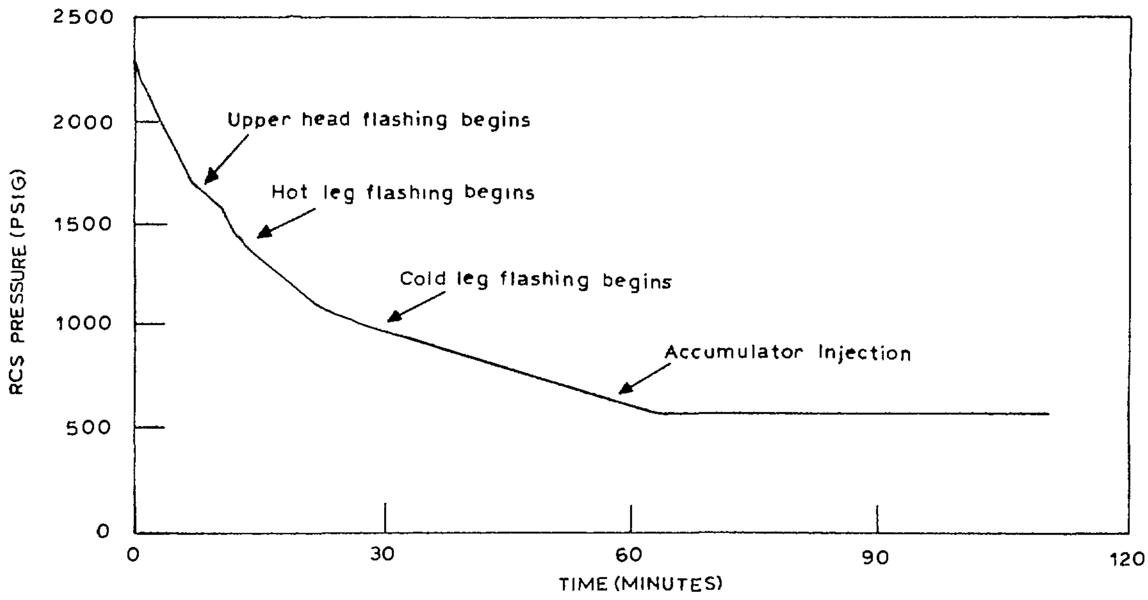


FIG. 6. RCS pressure — 300 gpm/RCP leakage with secondary depressurization.

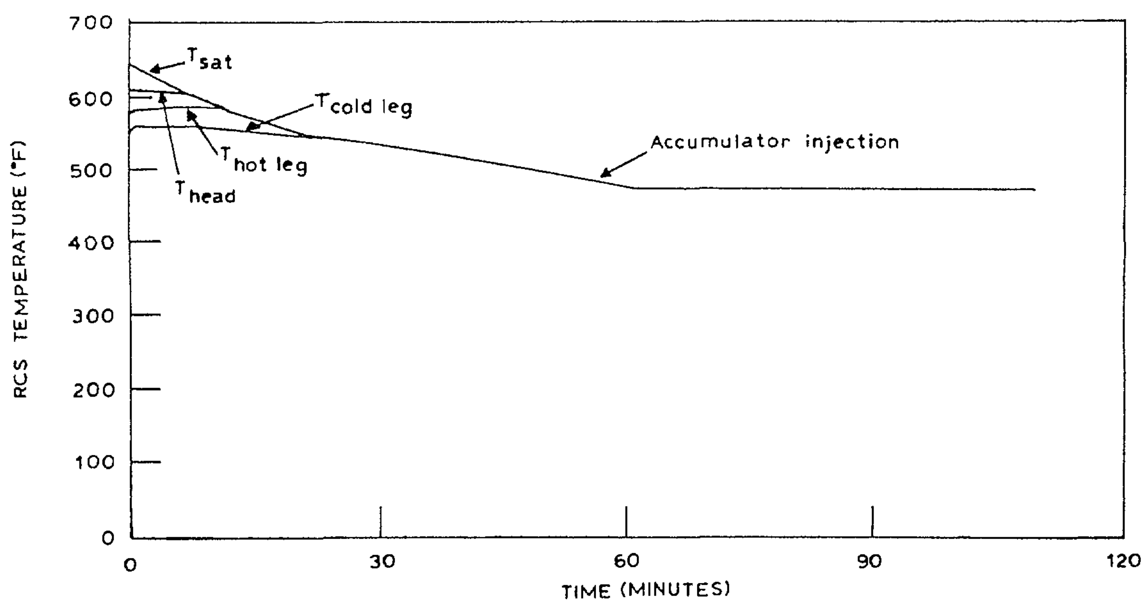


FIG. 7. RCS temperature — 300 gpm/RCP leakage with secondary depressurization.

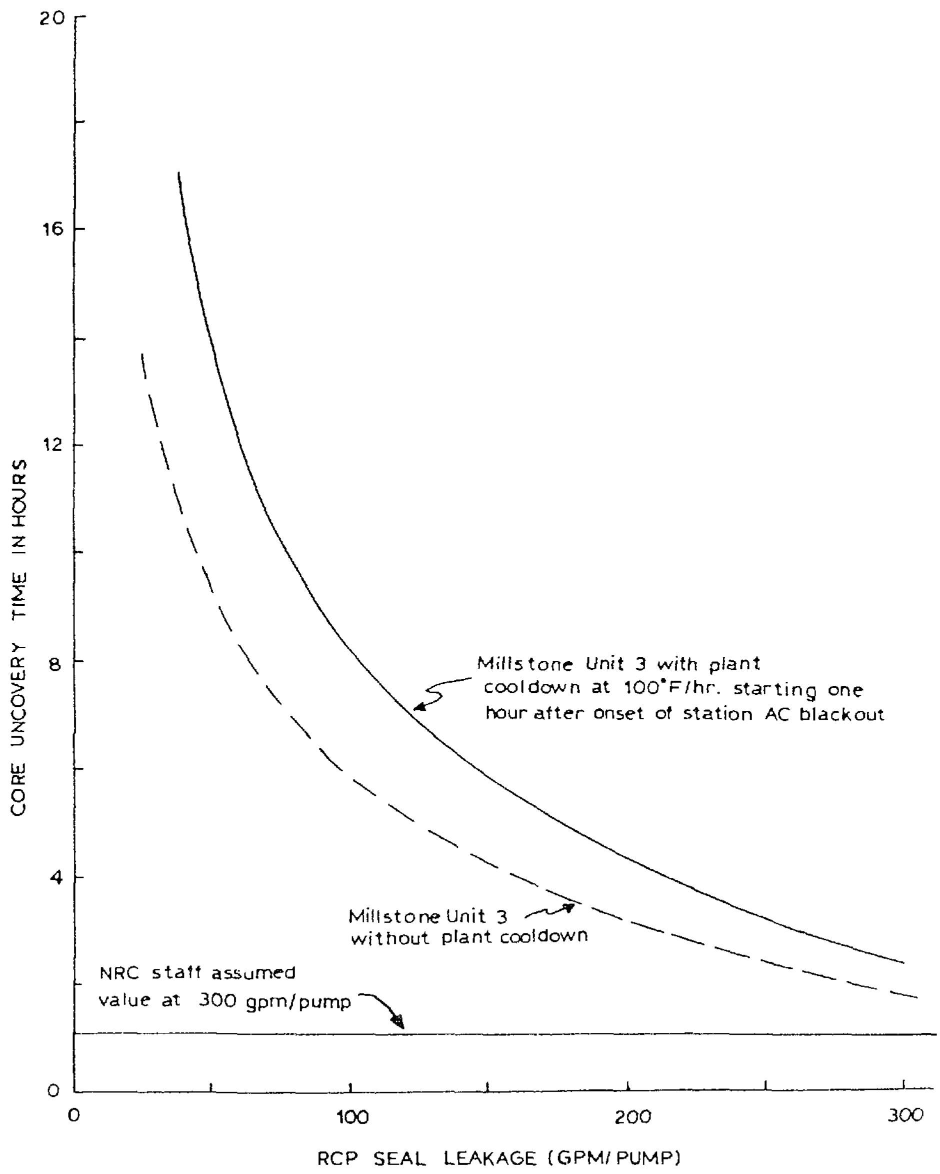


FIG. 8. Best-estimate core uncovery time following station AC blackout with RCP seal degradation.

Probability of Various Leakage Rates and Core Uncovery Times

The probability of various core uncovery times can be obtained based on knowledge of the likelihood of different leak rates and the plant specific response described in Figure 8. The quantification of the probability of different leak rates is discussed in Appendix A and is based on best estimate interpretation of the results of the work sponsored by the Westinghouse Owner's Group. The resultant distribution for core uncovery times is shown in Figure 9.

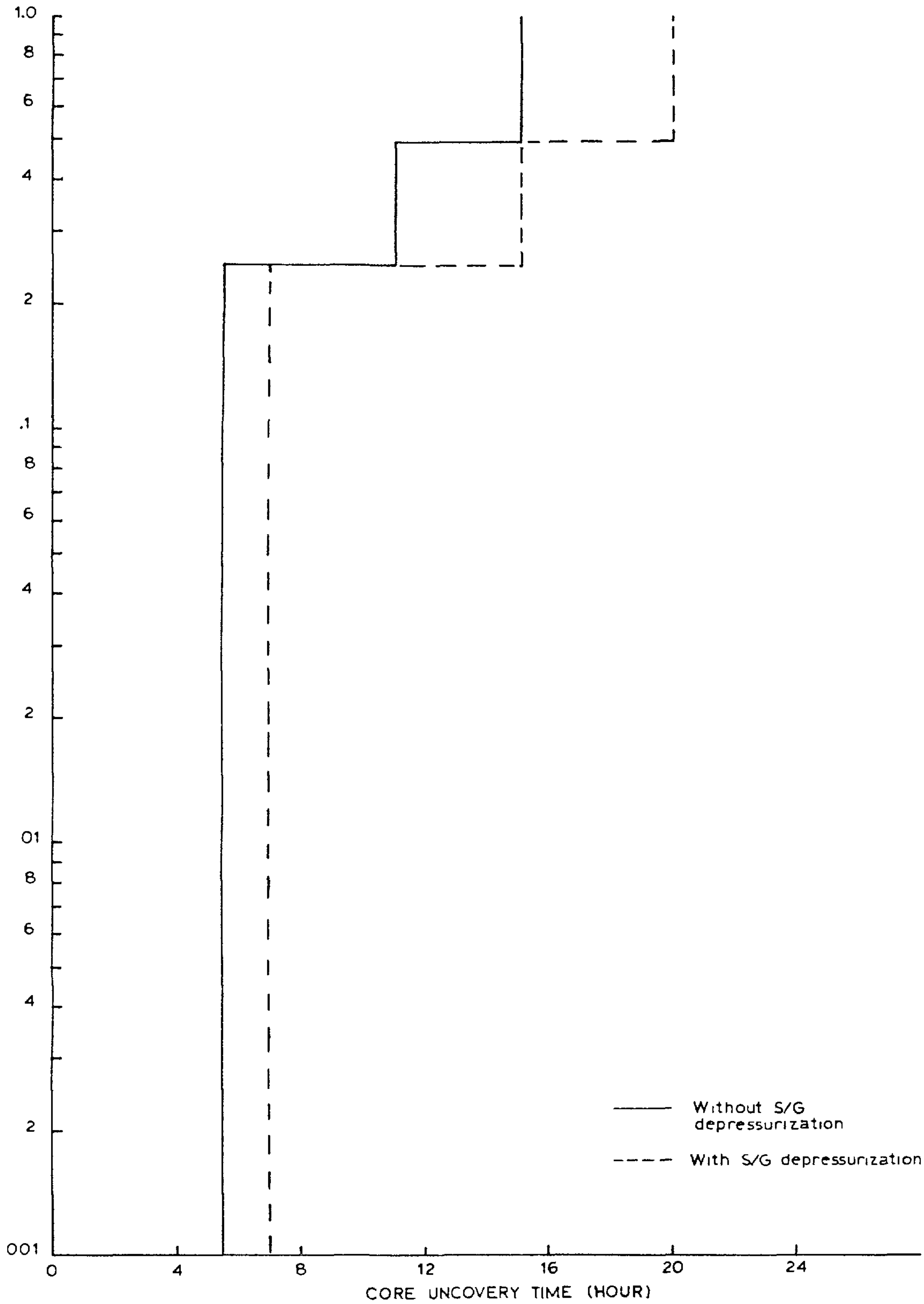


FIG. 9. Probability distribution for Millstone Unit 3 core uncovery time during station AC blackout.

4.1.2. Battery Depletion Time

In the Millstone Unit 3 FSAR (Reference 4) it is noted that using only equipment specifications and conservative industry battery sizing standards one would obtain a minimum 4 hour discharge time on each battery. To obtain a realistic upper bound estimate of battery depletion time at Millstone Unit 3, special test measurements were made on January 23, 1986. With the plant at hot standby conditions (DC electrical loads would be similar to what would exist during Station Blackout) measurements were made of the DC current drain to support all switchboard distribution loads. This load was increased by a 1.50 multiplier to conservatively account for momentary cyclic loads and possible future loads. The inverter load on the batteries was determined via measuring the AC load and converting this to the equivalent DC load with a 1.25 multiplier applied for conservatism. The acceptance criterion for battery depletion time was based on supplying minimum voltages to operate equipment at the end of the discharge period. The initial capacity of the batteries was additionally degraded to end of life conditions, wherein only 80% of rated capacity is available upon start of the discharge. Based on test measurements using these criteria the existing 1650 Amphour batteries, if subjected to a Station Blackout service profile, would have ample capacity to supply sufficient DC power for at least 8 hours. This would be true over the life of the batteries. This data equates to an 8 hour worst case battery depletion time or 95% value. (No battery capacity conservation measures are assumed.)

To obtain a best estimate or median battery depletion time, the conservative multipliers on the switchboard and inverter DC loads were removed and battery conservation efforts (initiated at 2 hours into the Station AC Blackout) were considered. The scope of battery conservation measures considered include: stripping of unnecessary DC loads, removing the inverters from the train batteries and running the inverters on the two channel batteries. A number of possible scenarios were considered which lead to a 12 hour best estimate value for battery depletion time.

The results of these test measurements and subjective interpretations are shown in Figure 10 which summarizes best estimate battery depletion time projections.

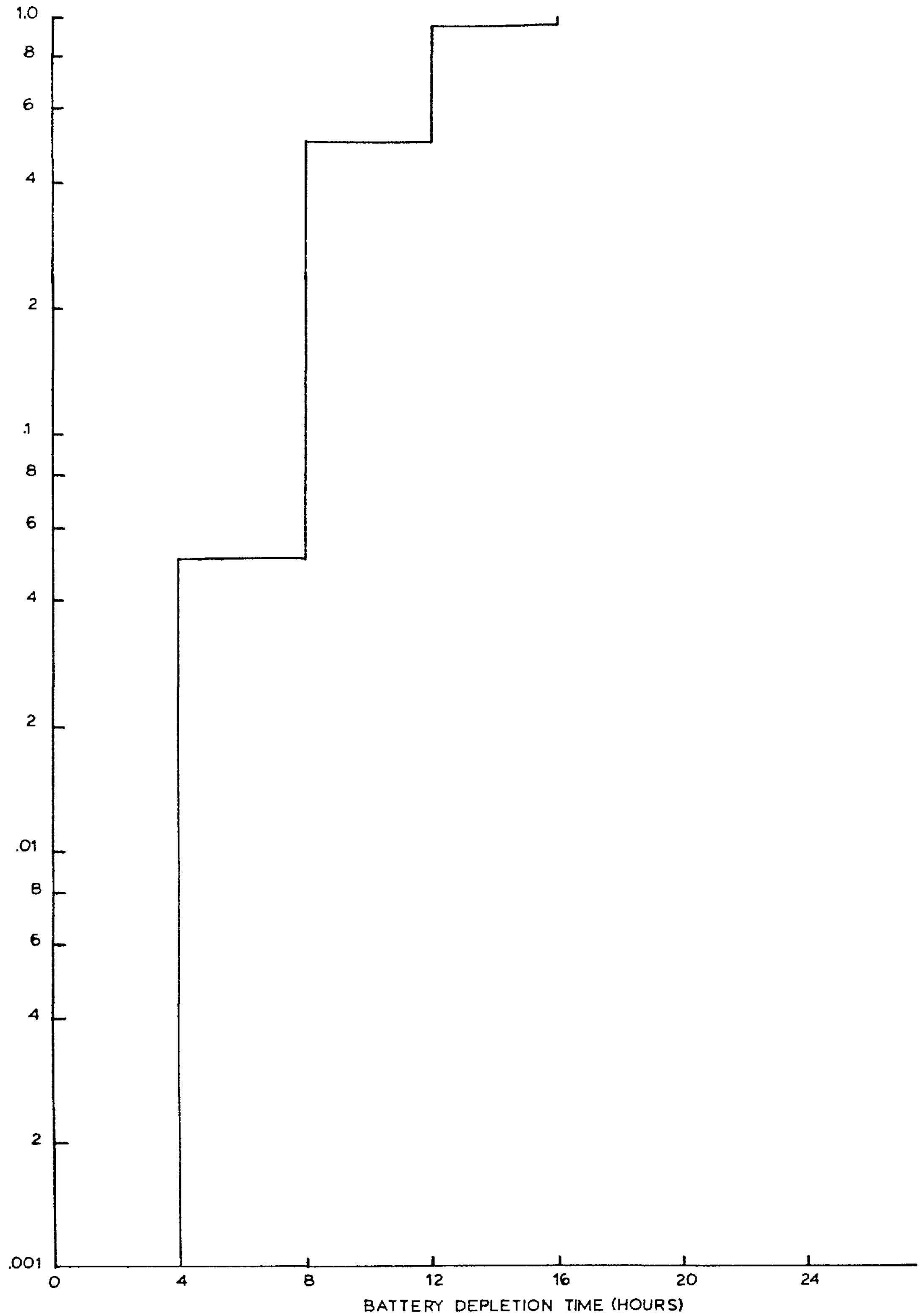


FIG. 10. Probability distribution for Millstone Unit 3 battery depletion during station AC blackout.

4.1.3. Impact of Loss of Switchgear Room Cooling on Coping Time

Following a complete loss of Station AC, all AC power related heat loads in the switchgear rooms at Millstone Unit 3 are eliminated and the flow of cool air drops off as the blower units coast down. The only remaining heat loads would be the heat rejected by the inverter units which convert DC power from the Station batteries to 120V AC for use in the Vital AC dependent systems. If the inverters (which will continue to run as long as DC power remains available from the batteries) reject sufficient heat to the switchgear rooms, the internal air temperature could increase to levels where the inverters could fail. Failure of an inverter will result in the loss of all associated 120V Vital AC loads. The key loads powered by the 120V Vital AC buses are the control board instruments which will be necessary to control the plant until Station AC is restored. Examples include: steam generator water level and pressure, RCS temperature and pressure, RCS subcooling, and the RVLMS.

To evaluate room heatup a multinode computer model was developed which considered the heat loss from the inverters as a heat source, and considered the massive concrete walls and ceilings as passive heat sinks. Best estimate calculations were performed along with a number of sensitivity calculations using worst limiting case values.

The inverter units at Millstone Unit 3 are 25kVA units manufactured by Elgar Controls of San Diego and are 80% efficient. The heat load from such an inverter under Station AC Blackout conditions would be 13,658 BTU/hr. Internal cooling for the inverter units is provided by 5 self-powered fans each rated at 560 cfm. Accounting for backpressure due to the tortuous air flow path and the intake air filters, the net cooling air flow would be roughly 800 cfm. The exhaust air from the inverter cabinet is directed toward the switchgear (on the 4'- 6" level) via a drip hood. Current test data indicate that the units can run for at least 8 hours in a 122°F environment which corresponds to a 134°F internal temperature.

The results of the switchgear room heatup calculations are shown in Figure 11. As noted, it takes 12 hours just to heat the room up to 100°F. The length of time required to fail the inverters due to loss of room cooling is thus evaluated as being so long that it does not represent any real consideration in the Station Blackout issue (i.e., other issues would tend to dominate).

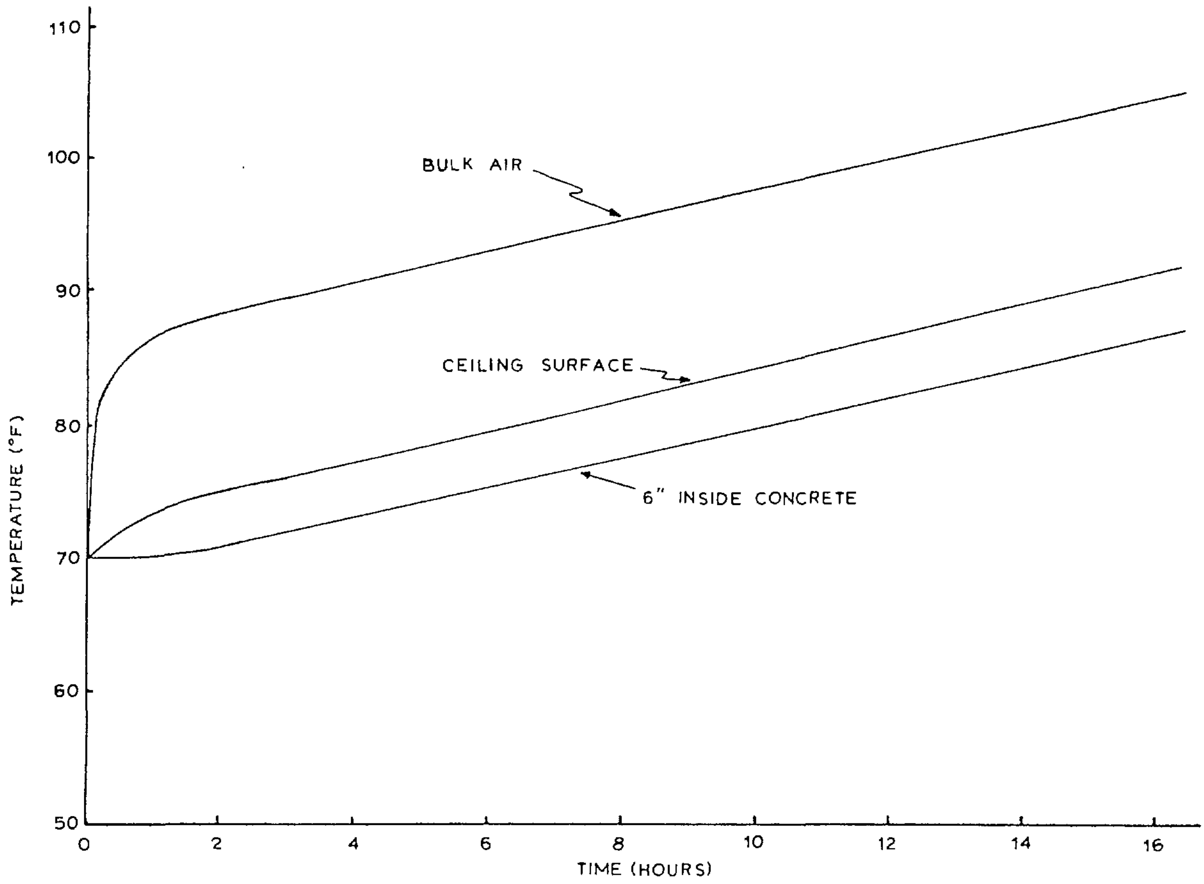


FIG. 11. Switchgear room heatup during station AC blackout.

4.1.4. Impact of Loss of AFW Pump Compartment Cooling on Coping Time

Following a Station Blackout, the availability of the steam driven auxiliary feedwater is critical in preventing severe core damage. If the steam driven auxiliary feedwater pump should fail, the loss of decay heat removal from the RCS would cause repressurization of the RCS to the point that the pressurizer PORVs would open. This would result in a long term loss of coolant inventory without the capability to provide makeup.

Upon careful review of the design basis of the steam driven auxiliary feedwater pump, it was determined that the existing equipment is actually designed to operate under conditions of a long term sustained

Station Blackout. Amendment 13 to the Millstone Unit 3 FSAR (Reference 4) notes that a 12 hour sustained 162°F room temperature environment was used to bound the Maximum Abnormal Excursion (MAE) and states:

> "The transient Maximum Abnormal Excursion is based on the requirement to have the turbine-driven auxiliary feedwater pump operative through a complete loss of all AC power."

Based on this it may be concluded that the loss of room cooling which is a direct consequence of a Station AC Blackout, will not result in loss of the steam driven auxiliary feedwater pump. The impact of this on the Station AC Blackout core damage frequency models is that auxiliary feedwater flow availability does not have to enter into considerations of the "grace time" available before the onset of severe core damage.

4.1.5. Determination of Overall Station Blackout Coping Time

There are two competing effects which determine the coping time during a Station Blackout event:

- o Rate of degradation of the RCP sealing system
- o Rate of depletion of the Station Batteries.

A composite discrete probability distribution (DPD), representing both DC power and RCP seal integrity related coping time, was then generated using the following formula:

$$P(\tau_i) = P_{RCP}(\tau_i) + P_{DC}(\tau_i) - P_{RCP}(\tau_i)P_{DC}(\tau_i)$$

The resultant discrete probability distribution and density function of coping times (in hours) is as follows:

τ_i	$P(\tau_i)$	$w(\tau_i)$
1.5	4.3×10^{-5}	4.3×10^{-5}
4.0	5.0×10^{-2}	5.0×10^{-2}
5.5	2.9×10^{-1}	2.37×10^{-1}
8.0	6.2×10^{-1}	3.38×10^{-1}
12.0	9.7×10^{-1}	3.5×10^{-1}
15.0	1.0×10^{-0}	2.5×10^{-2}

The generation of this composite distribution from the other two distributions is shown in Figure 12. Using this distribution a mean coping time of 8.78 hours was obtained using DPD arithmetic.

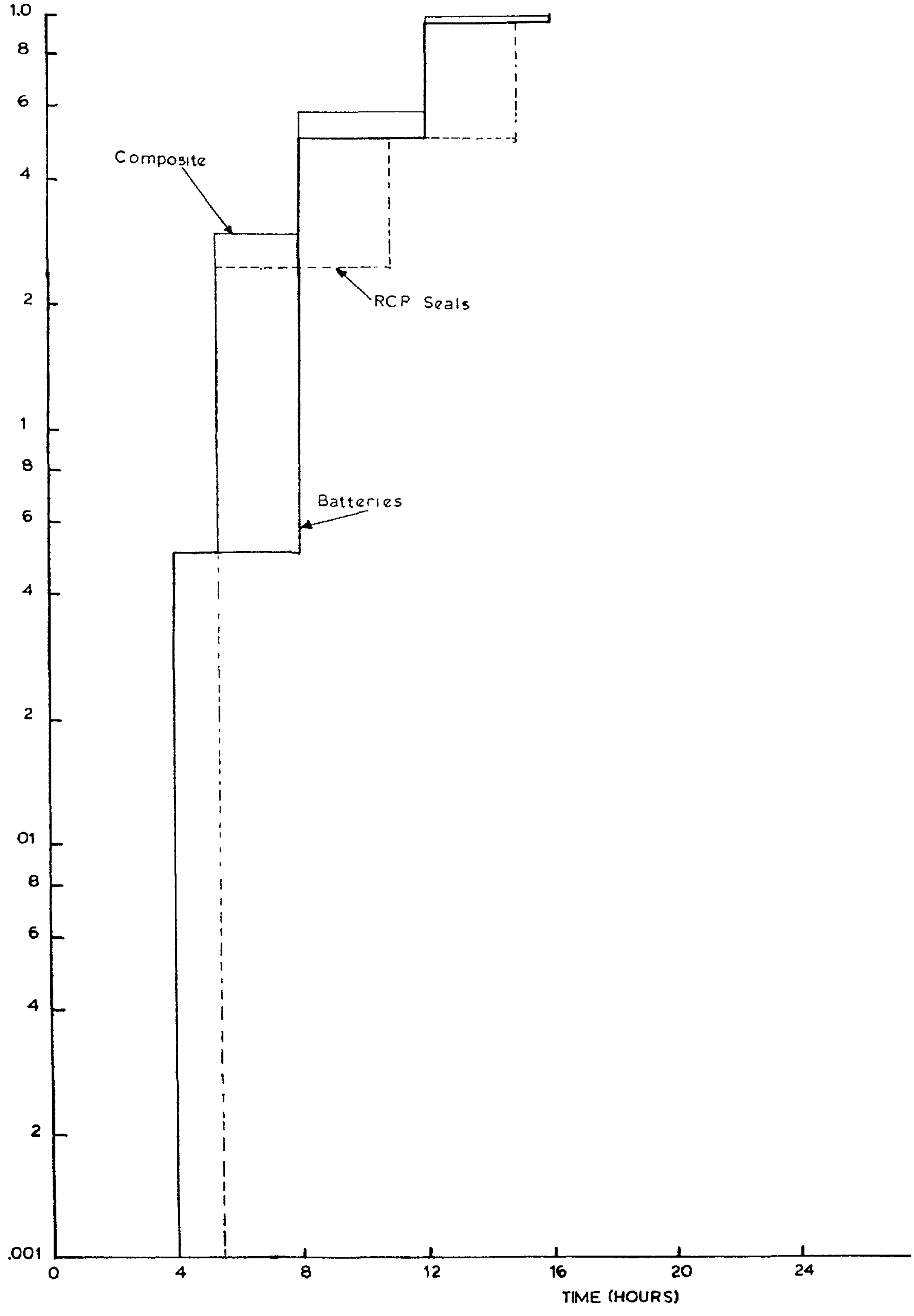


FIG. 12. Coping time distribution.

4.2. Core Damage Frequency Model

4.2.1. Station Blackout Fault Tree Model

To develop a Station Blackout quantification model it is necessary to begin with a fault tree describing the possible failure scenarios which can result in the Station Blackout condition. Figure 13 shows a simplified fault tree showing all possible combinations of event which could result in Station Blackout. Note that failures of critical support systems have been screened out based on the plant specific design. The Millstone Unit 3 Service Water system is composed of two redundant trains each with two service water pumps. On loss of offsite power with subsequent diesel start, both service water pumps in each train are signaled to start (i.e.: 2/2 redundancy in pumps, valving, and piping). This effectively makes diesel unavailability due to insufficient service water cooling significantly less likely than diesel failures by themselves.

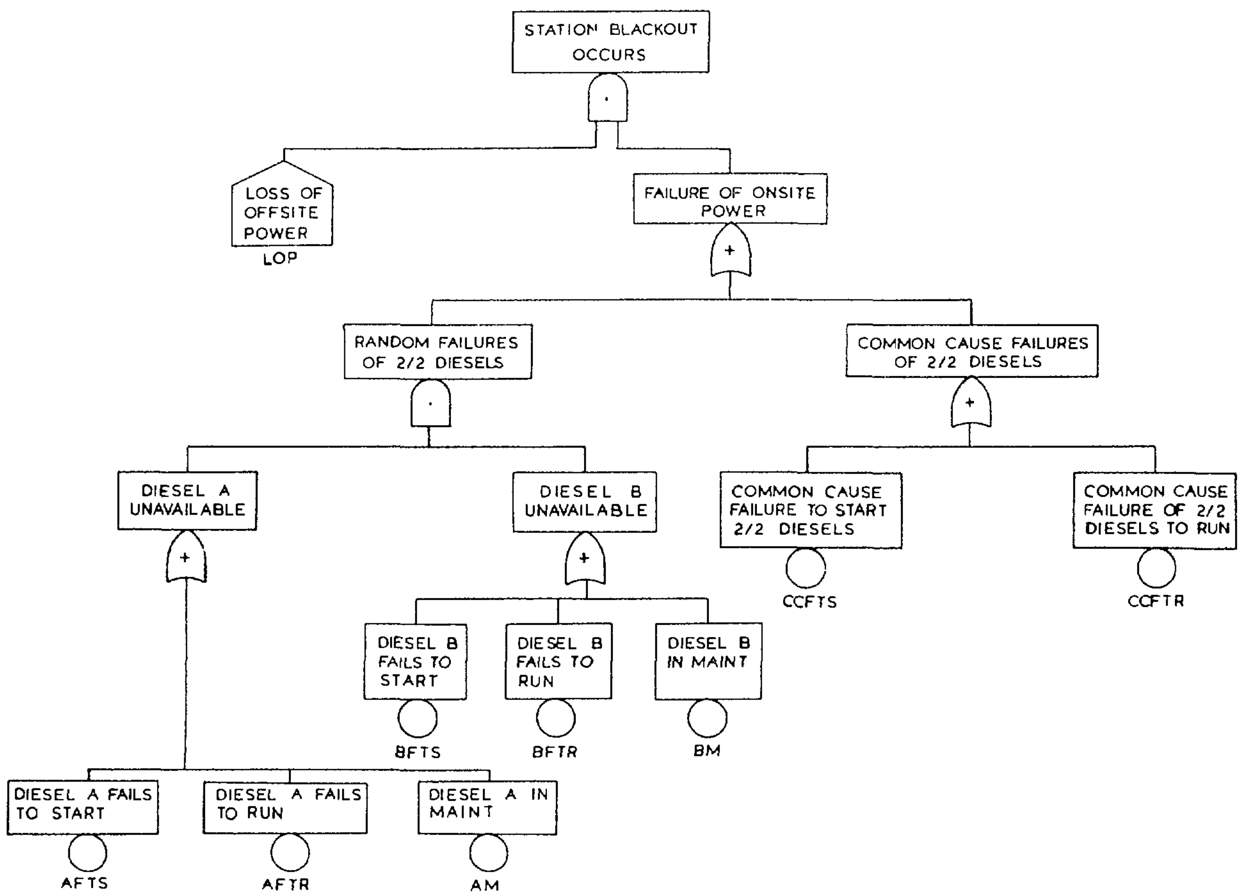


FIG. 13. Station blackout fault tree.

Reducing the Boolean logic the following cutsets are obtained:

SBO = LOP * CCFTS
LOP * CCFTR
LOP * AFTS * BFTS
LOP * AFTS * BFTR
LOP * AFTS * BM
LOP * AFTR * BFTS
LOP * AFTR * BFTR
LOP * AFTR * BM
LOP * AM * BFTS
LOP * AM * BFTR

The cutset LOP * AM * BM is eliminated due to restrictions in Technical Specifications not permitting both diesels to be simultaneously in maintenance at the same time.

To develop a time-dependent model the various cutsets are first sorted into cases related to the timing of the various scenarios. This is shown below.

Case 1 - One diesel in maintenance, loss of offsite power occurs, and the redundant diesel fails to start. None of the possible power sources is recovered within the plant's coping time.

LOP * [AM * BFTS + AFTS * BM]

Case 2- One diesel in maintenance, loss of offsite power occurs, and the second diesel starts but subsequently fails to run. None of the possible power sources is recovered within the plant's coping time.

LOP * [AM * BFTR + BM * AFTR]

Case 3- Loss of offsite power occurs and both diesels fail to start either due to random or common cause faults. None of the possible power sources is recovered within the plant's coping time.

LOP * [AFTS * BFTS + CCFTS]

Case 4- Loss of offsite power occurs, one diesel fails to start, and the other diesel starts but subsequently fails to run. None of the possible power sources is recovered within the plant's coping time.

LOP * [AFTS * BFTR + AFTR * BFTS]

Case 5- Loss of offsite power occurs, both diesels start, but at some time later both simultaneously fail to run as a result of common cause failures. None of the possible power sources is recovered within the plant's coping time.

LOP * CCFTR

Case 6- Loss of offsite power occurs, both diesels start, one diesel fails to run, and subsequently at a later time the second diesel fails to run due to a different cause. None of the possible power sources is recovered within the plant's coping time.

LOP * AFTR * BFTR

4.2.2. Time-Dependent Cutset Quantification

The time-dependent probability expressions for each of the basic events in the cutset expressions may be defined as follows:

$\lambda_n \exp(-\lambda_n t)$ probability of a loss of offsite power occuring within "t" hours

$Q_n(t)$ probability of non-recovery of offsite power within "t" hours after loss of offsite power

$\lambda_m \exp(-\lambda_m t)$ probability of a maintenance outage having to be initiated within "t" hours

$Q_m(t)$ probability of non-recovery from maintenance within "t" hours of starting a maintenance action

q_f — probability of a diesel failing to start on demand due to random failures

$\lambda_f \exp(-\lambda_f t)$ — probability of a running diesel failing to continue running within "t" hours of starting

$Q_f(t)$ — probability of non-recovery of a failed diesel within "t" hours after failing

q_{cc} — probability of two diesels failing due to common cause failure to start

$\lambda_{cc} \exp(-\lambda_{cc} t)$ — probability of two running diesels failing to continue to run within "t" hours of starting

$Q_{cc}(t)$ — probability of non-recovery from common cause diesel failure

The time-dependent probability expressions for each of the cutsets is shown below.

Case 1- One diesel is in maintenance, loss of offsite power occurs, and the redundant diesel fails to start. The probability of this condition lasting longer than τ_i hours is expressed:

$$\mathrm{Prob}\{LOP*[AM*BFTS + AFTS*BM] \mid t > \tau_i\} =$$

$$2\lambda_m q_f \int_0^{+\infty} \lambda_n \exp(-\lambda_n t) Q_n(\tau_i) Q_f(\tau_i) Q_m(t+\tau_i)\,dt$$

In this integral expression the coping time τ_i is treated as a dummy variable. The probability of non-recovery of offsite power and non-recovery of the failed diesel $(Q_n(\tau_i)Q_f(\tau_i))$ is assessed at the coping time whereas the probability of non-recovery from maintenance is assessed for a longer time period (namely: $t + \tau_i$). The overall integration over "t" reflects the probability of an unfinished maintenance act on one of the diesels with a loss of offsite power occurring. The other integral expressions are developed similarly.

Case 2- One diesel is in maintenance, loss of offsite power occurs, and the second diesel starts but subsequently fails to run. The probability of this condition lasting longer than τ_i hours is expressed:

$$\mathrm{Prob}\{LOP^*[AM^*BFTR + AFTR^*BM] \mid t>\tau_i\} =$$

$$2\lambda_m \int_0^{+\infty}\int_t^{+\infty} \lambda_f \exp(-\lambda_f x)\, Q_f(\tau_i)\, Q_m(x+\tau_i)\, \lambda_n \exp(-\lambda_n t)\, Q_n(x-t+\tau_i)\, dx\, dt$$

Case 3- Loss of offsite power occurs and both diesels fail to start either due to random or common cause faults. The probability of this condition lasting longer than τ_i hours is expressed:

$$\mathrm{Prob}\{LOP^*[AFTS^*BFTS + CCFTS] \mid t> \tau_i\} =$$

$$\lambda_n [q_f^2 Q_f(\tau_i)^2 + q_{cc} Q_{cc}(\tau_i)] Q_n(\tau_i)$$

Case 4- Loss of offsite power occurs, one diesel fails to start, and the other diesel starts but subsequently fails to run. The probability of this condition lasting longer than τ_i hours is expressed:

$$\mathrm{Prob}\{LOP^*[AFTS^*BFTR + AFTR^*BFTS] \mid t>\tau_i\} =$$

$$2\lambda_n q_f Q_f(\tau_i) \int_0^{+\infty} \lambda_f \exp(-\lambda_f w) Q_f(w+\tau_i) Q_n(w+\tau_i)\, dw$$

Case 5- Loss of offsite power occurs, both diesel start, but after some period of time later both simultaneously fail to run as a result of common cause failures. The probability of this condition lasting longer than τ_i hours is expressed:

$$\mathrm{Prob}\{LOP^*CCFTR \mid t>\tau_i\} =$$

$$\lambda_n \int_0^{+\infty} \lambda_{cc} \exp(-\lambda_{cc} w) Q_{cc}(\tau_i) Q_n(w+\tau_i)\, dw$$

Case 6- Loss of offsite power occurs, both diesels start, one diesel fails to run, and subsequently at a later time the second diesel fails to run due to a different cause. The probability of this condition lasting longer than τ_i hours is expressed:

$$\text{Prob}\{\text{LOP*AFTR*BFTR} \mid t>\tau_i\} =$$

$$= 2\lambda_n \int_0^{+\infty} \int_x^{+\infty} \lambda_f \exp(-\lambda_f x)\, Q_f(t-x+\tau_i)\, Q_n(x+\tau_i)\, \lambda_f \exp(-\lambda_f t)\, Q_f(\tau_i)\, dt\, dx$$

In the above expression it is important to note the factor of x2 in front of the integrations. This factor acknowledges the fact that two possible paths exist depending on which running diesel fails first.

To quantify the above time-dependent models it is necessary to develop estimates of the various functions involved based on plant and industry statistics. This estimation is discussed in the following section.

4.2.3. Frequency of Loss of Offsite Power at the Millstone Site

The Millstone Unit 3 PSS (Reference 5, p.1.1-29), submitted in 1983, calculated a mean Millstone site loss of offsite power frequency of 1.1×10^{-1}/yr using Bayesian statistics with a prior distribution obtained from industry loss of offsite power experience. This was updated with 13 years of Millstone site experience during which time there was one loss of offsite power event, during Hurricane Belle in 1976.

The Millstone Unit 1 PSS (Reference 6, p. 1.2-8), issued in July 1985, calculated a mean Millstone site loss of offsite power event frequency of 1.24×10^{-1}/yr. This revised Bayesian statistics calculation was based exclusively on northeastern regional experience obtained from Northeast Power Coordinating Council (NPCC) data. This prior data was updated with 14 years of Millstone site experience again with only the Hurricane Belle event. The slight increase in frequency is a result of using more regional statistics and a slightly larger plant experience data base.

An updated estimate of the site specific loss of offsite power frequency can be obtained via performing a Bayesian statistical calculation using NPCC regional data updated with 15 years of Millstone site experience in which there were two events: Hurricane Belle in 1976 and Hurricane Gloria in 1985. The nature of the Gamma distributed prior distribution is discussed in Reference 6. The results of the Bayesian update are as follows:

$$\lambda_n = 1.45 \times 10^{-1}/\text{yr.}$$

$$\text{Var } \lambda_n = 3.92 \times 10^{-3}/\text{yr.}^2$$

The results are similarly assumed to be Gamma distributed.

4.2.4. Offsite Power Restoration Times at the Millstone Site

The distributions of offsite power recovery times used in the Millstone Unit 3 PSS (Reference 1) were based on very limited data available at the time that the study was performed. Despite this, it compares reasonably well with analogous data contained in NUREG-1032 (Reference 7, p. A-39). The key differences are related to an assumption that some finite probability for non-restoration exists for very long time frames.

The issue of offsite power restoration times was reevaluated in the Millstone Unit 1 PSS (Reference 6, p.2A-5) which was issued in July 1985. The Millstone Unit 1 PSS developed a cumulative distribution for restoration times for nuclear plant sites in the NPCC region based on NSAC data contained in Reference 8. This cumulative distribution included only the effects of Hurricane Belle in 1976. The results of this analysis of industry data is shown in Figure 14.

To evaluate the impacts of Hurricane Gloria on the assumed mean restoration time, an evaluation was performed of what time period would be required to restore offsite power to Millstone Unit 3 had emergency conditions existed at the time. Reference 9 documented the fact that although offsite power was not promptly recovered at the Millstone site - it could have been had conditions warranted. Reference 9 did not address Millstone Unit 3 power recovery because the unit was not operational and had no fuel in

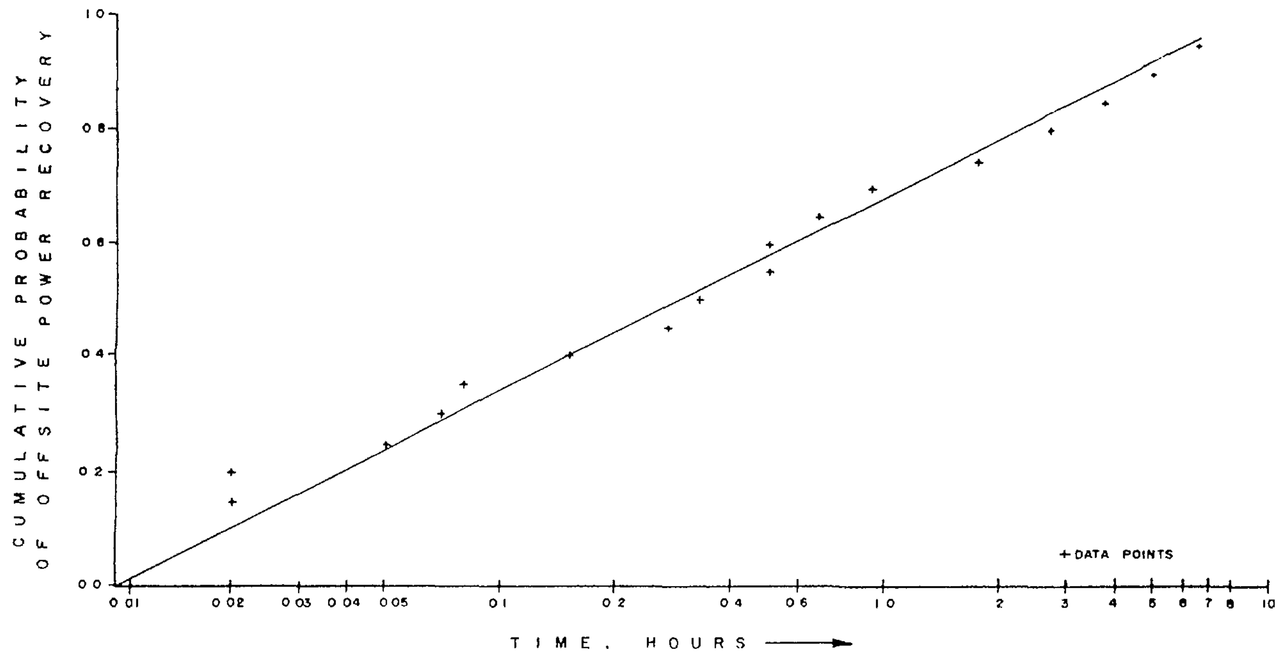


FIG. 14. Recovery of off-site power.

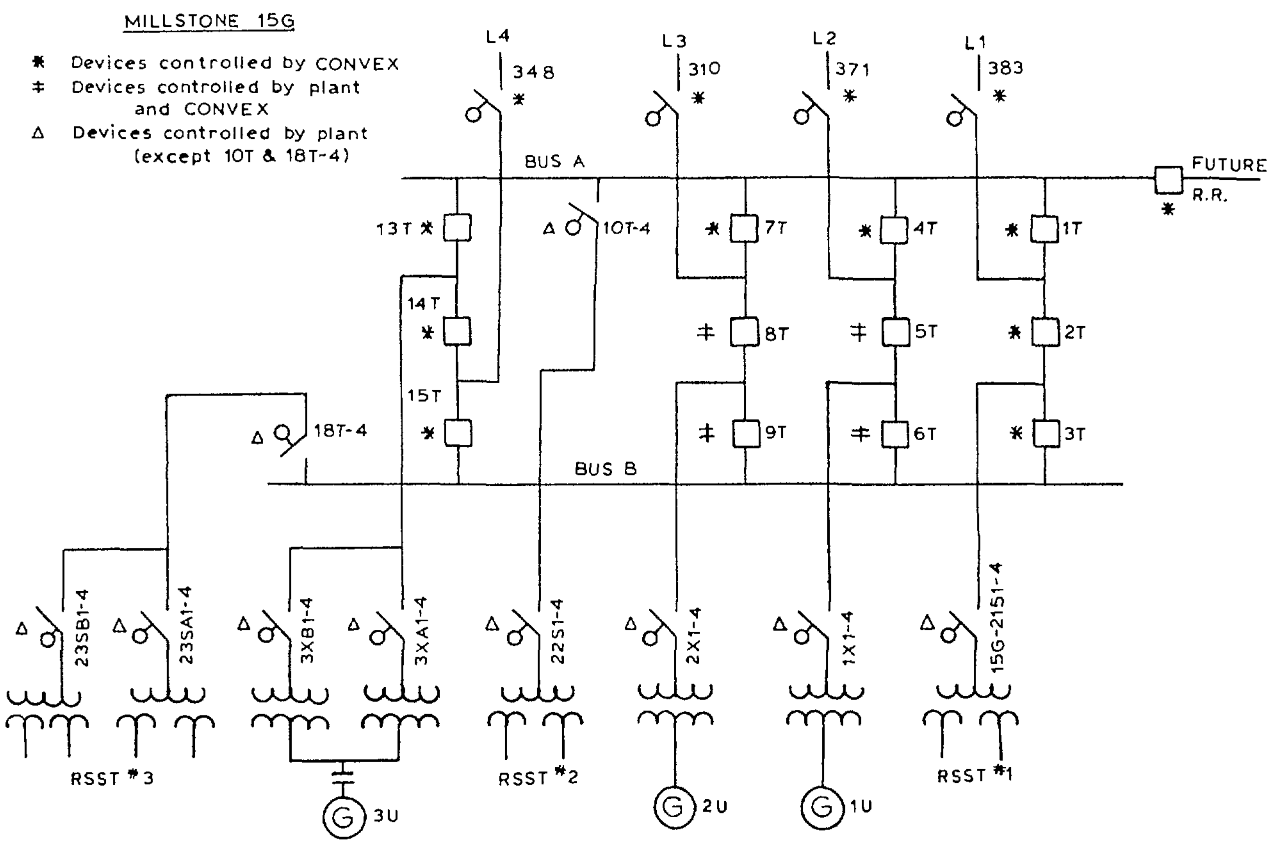


FIG. 15. Millstone switchyard.

the reactor. An evaluation has since been performed to determine what the restoration time at Millstone Unit 3 could have been had it been necessary.

Figure 15 shows a simplified One Line Diagram of the Millstone site switchyard. It is important to recognize that throughout the Hurricane Gloria event power from the 345kV grid (via the #348 circuit) was available. The same is true of Hurricane Belle in 1976. To reconnect Millstone Unit 3 to the offsite power grid it would be necessary to perform the following actions:

- o Washdown all conducting surfaces between breakers 13T and 15T. (It is not necessary to washdown the main North-South bus ducts. The washdown of these bus ducts under non-emergency conditions is one of the prime causes for the duration of the Millstone site switchyard outage.)

- o Washdown conducting surfaces associated with the Millstone Unit 3 Main Generator Stepup Transformers and 345kV takeoff structures.

- o Open breakers 13T and 15T. This isolates the potentially salt coated bus ducts and insulators which could result in ground faults.

- o Close the main disconnect between the Millstone switchyard and 345kV line #348.

- o Re-energize 345kV line #348 from the remote end of the line.

- o Assure the Main Generator Breaker on the Millstone Unit 3 generator is open and the disconnect switches on the Main Generator Stepup Transformers are closed.

- o Close breaker 14T thus powering the Millstone Unit 3 auxiliaries via backfeeding through the Generator Stepup Transformer.

An evaluation performed of these steps by Northeast Utilities has lead to a conclusion that the entire restoration could have been accomplished in roughly a two hour time period from the time started. Based on weather conditions experienced at the time, it is estimated that such restoration could have been initiated (had conditions warranted) in 1.5 hours after the initial loss of offsite power. This results in an overall estimate of 3.5 hours to restore offsite power to the Millstone Unit 3 auxiliaries.

This additional data point was used to update the cumulative distribution of recovery times used in Reference 6. As would be expected, inclusion of the 3.5 hour data point for Hurricane Gloria causes an increase in the predicted mean restoration time. Using this cumulative distribution for recovery, a cumulative distribution for failure to recover offsite power $Q_n(t)$ was then developed.

To facilitate closed form evaluation of the convolution integrals in the Station Blackout core damage frequency model, this cumulative distribution function was fitted to a linear sum of two exponential terms:

$$Q_n(t) = A \exp(-at) + B \exp(-bt)$$

-where: $A = 0.4$ $\quad a = 0.297$

$B = 0.6$ $\quad b = 4.6$

The first term of this expression is asymptotic to the long term restoration trend, whereas the second term (which drops off quickly) describes the short term restoration effects. Our review of this distribution function shows that it is conservative for short restoration times (higher non-recovery probabilities are predicted), provides a reasonably accurate best-estimate result for recovery times in the 1.0 to 5.0 range, and becomes conservative for restoration times greater than 5.0 hours.

4.2.5. Treatment of Diesel Reliability Data

At the time this analysis was undertaken Millstone Unit 3 had just recieved authorization to perform low power testing. Hence there was insufficient plant specific reliability data. In order to perform reliability calculations, data from other Northeast Utilities operating nuclear power plants was used. This is discussed below.

Diesel Unavailability on Demand

Regulators assumed a diesel unavailability on demand of $q_f = 3 \times 10^{-2}$/demand based on NUREG/CR-2728 (Reference 10). Based on Northeast Utilities operating experience, the diesel unavailability value chosen by regulators was felt to be excessively conservative to the point where conclusions regarding needs for hardware modifications would be driven solely by conservative assumptions. Detailed reliability analyses already performed for the diesels of two of our operating nuclear power plants are shown in Figure 16. (This data for the Millstone Unit 1 and Connecticut Yankee diesels had already been

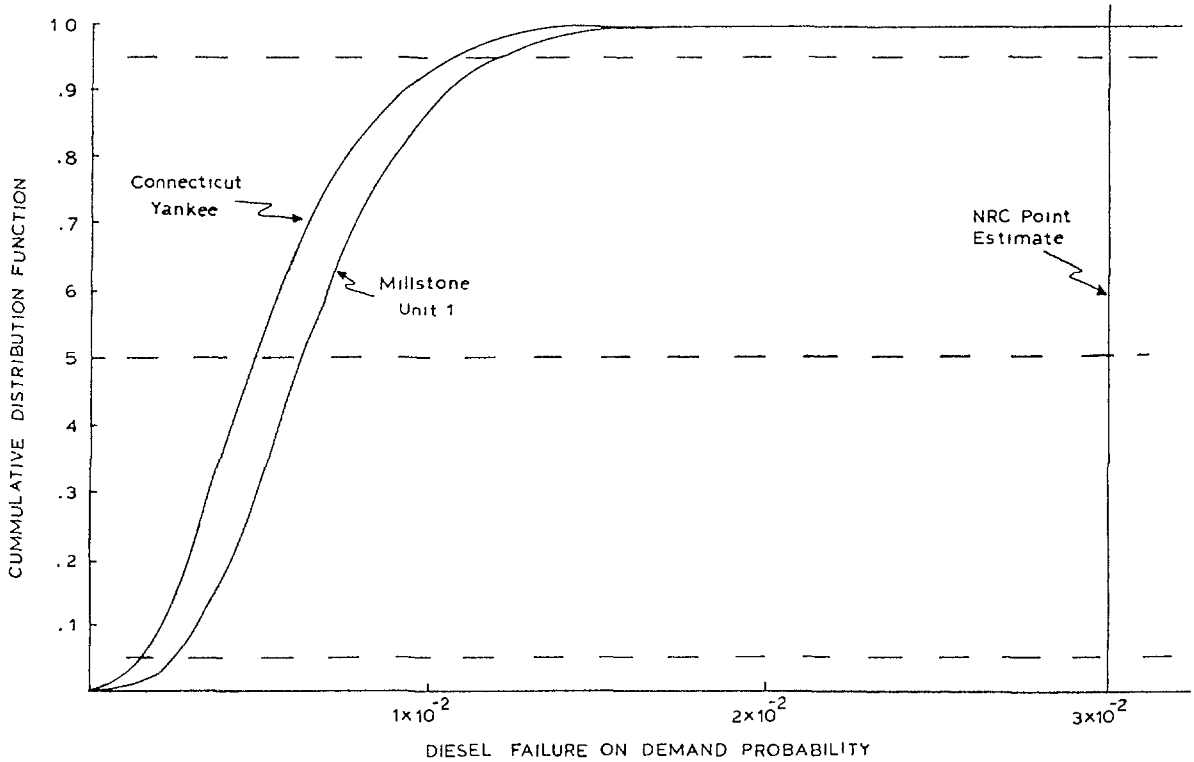


FIG. 16. Comparison of diesel failure on demand probabilities.

audited and reviewed by regulators and their consultants.) Also shown on this figure is the regulators suggested value which is a point estimate without uncertainties. In our opinion it was unlikely that the future diesel experience at Millstone Unit 3 will be significantly different from the Connecticut Yankee and Millstone Unit 1 experience.

Diesel generator reliability experience from the Millstone Unit 1 and Connecticut Yankee diesels is summarized below and is compared to the regulators estimate.

Data Source	Mean q_f	Var q_f
NUREG/CR-2728	3.0×10^{-2}	----------
Millstone Unit 1 PSS	6.7×10^{-3}	9.6×10^{-6}
Connecticut Yankee PSS	5.4×10^{-3}	7.7×10^{-6}

In performing uncertainty analysis it was assumed that the unavailabilities were Beta distributed.

Diesel Failure to Run Given Successful Start

Regulators assumed a diesel failure rate to continue running given successful start of $\lambda_f = 3.0 \times 10^{-3}$/hr based on NUREG/CR-2815 (Reference 11, Table C.1). Our review of the origins of the value suggested by regulators pointed out there were shortcomings in the data because it was mainly based on engineering judgement. The referenced Table C.1 of NUREG/CR-2815 under item C.3 "Shortcomings of the Data Table" stated:

> "In all likelihood, modifications of this table (C.1) will be necessary from time to time, ... because of new insights gained from operational experience.."

Based on Northeast Utilities operating experience, a value of $\lambda_f = 3.0 \times 10^{-3}$/hr (as a best-estimate for the Millstone Unit 3 diesel) was viewed as excessively conservative. Detailed reliability analyses already performed for the diesels of two of our operating nuclear power plants are shown in Figure 17. (The data for the Millstone Unit 1 and Connecticut Yankee diesels had already been audited by the regulators and their consultants.) Also shown overlayed on this figure is the regulators' suggested value. In our opinion it was highly

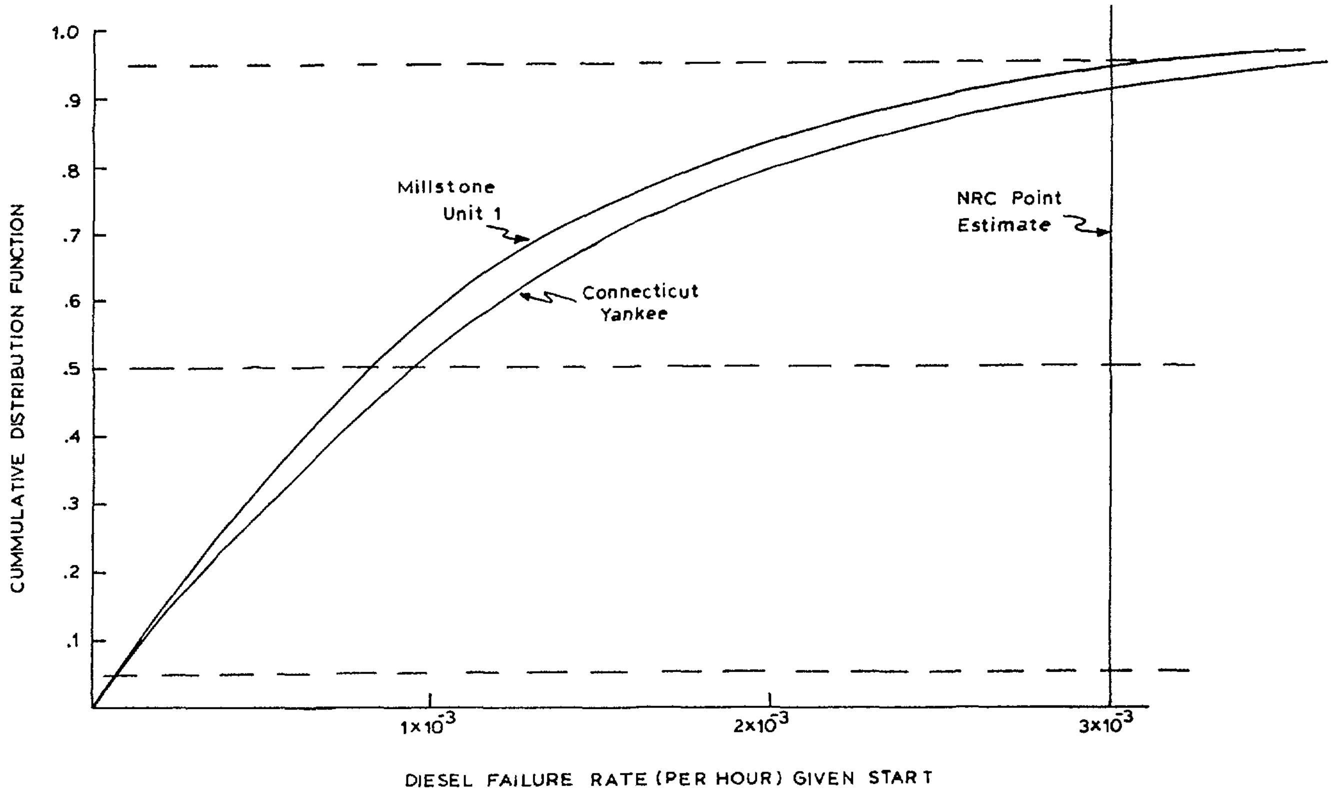


FIG. 17. Comparison of diesel failure rates given start.

unlikely that the future Millstone Unit 3 diesel experience would be significantly different from the Connecticut Yankee and Millstone Unit 1 experience.

Diesel generator reliability experience for the Millstone Unit 1 and Connecticut Yankee diesels is summarized below.

Data Source	Mean λ_f	Var λ_f
NUREG/CR-2815	3.0×10^{-3}/hr	---------
Millstone Unit 1 PSS	1.1×10^{-3}/hr	1.1×10^{-6}
Connecticut Yankee PSS	1.3×10^{-3}/hr	1.4×10^{-6}

For the purposes of performing uncertainty analysis it was assumed that the failure rate data was Gamma distributed.

Diesel Common Cause Failure to Start Probability

Regulators in their analysis of the problem assumed a diesel common cause failure to start probability of $q_{cc} = 1.1 \times 10^{-3}$ on demand. Northeast Utilities had no plant specific data base at the time for estimating common cause failure rates, but, using common cause failure rates conservatively derived using Licensee Event Reports (or LERs), data sources such as NUREG/CR-2099 (Reference 12) would yield: $q_{cc} = 2.59 \times 10^{-4}$ (based on the Binomial common cause failure rate model this value would be Gamma distributed). The regulators suggested value was thus found to be a factor of x4.2 larger than published data would suggest.

Diesel Maintenance Unavailability

Unavailability due to maintenance is characterized by the mean frequency of maintenance activities which temporarily render the diesels unavailable, and by the mean time to restore the diesel from the maintenance. Based on a review of maintenance records, Northeast Utilities was able to make the following estimates:

$$\lambda_m = 5.25 \times 10^{-5}/\text{hr} \qquad \text{Var } \lambda_m = 2.76 \times 10^{-9}$$

These values are assumed to be Gamma distributed.

The mean time to restore the diesel from failures was estimated by regulators as about 15 hours. Assuming exponential distributions, the non-recovery probabilities from either maintenance or actual failures during tests could then be expressed as follows:

$$Q_m(t) = \exp(-t/15)$$

$$Q_f(t) = \exp(-t/15)$$

4.3. Quantification of the Core Damage Frequency Model

In the previous sections a Station Blackout core damage frequency model was developed based on time-dependent PSA calculations. This section describes the techniques used to quantify this model.

4.3.1. Integration of the Time-Dependent PSA Models

By making the proper choice of model expressions it is possible to directly evaluate all of the integral expressions in closed form thus setting up expressions which can be further evaluated in the uncertainty analysis using standard Monte Carlo sampling techniques. The results of the integrations are shown below.

Case 1- Prob{LOP*[AM*BFTS + AFTS*BM]| $t>\tau_i$} =

$$2\lambda_m q_f \int_0^{+\infty} \lambda_n \exp(-\lambda_n t) Q_n(\tau_i) Q_f(\tau_i) Q_m(t+\tau_i) dt$$

$$= 2\lambda_n[\lambda_m q_f[\lambda n/(\lambda_n + \alpha)]\exp(-2\alpha\tau_i)[A\ \exp(-a\tau_i) + B\ \exp(-b\tau_i)]$$

Case 2- Prob{LOP*[AM*BFTR + AFTR*BM]| $t>\tau_i$} =

$$2\lambda_m \int_0^{+\infty}\int_t^{+\infty} \lambda_f \exp(-\lambda_f x) Q_f(\tau_i) Q_m(x+\tau_i) \lambda_n \exp(-\lambda_n t) Q_n(x-t+\tau_i) dx dt$$

$$= 2[\lambda_m\lambda_n\lambda_f/(\lambda_f + \lambda_n + \alpha)]\{A[\exp(-(2\alpha + a)\tau_i)]/[\lambda_f + \alpha + a]$$

$$+ \quad B[\exp(-(2\alpha + b)\tau_i)]/[\lambda_f + \alpha + b)]\}$$

Case 3- Prob{LOP*[AFTS*BFTS + CCFTS]| $t>\tau_i$} =

$$\lambda_n[q_f^2 Q_f(\tau_i)^2 + q_{cc} Q_{cc}(\tau_i)] Q_n(\tau_i)$$

$$= \lambda_n[q_f^2 \exp(-2\alpha\tau_i) + q_{cc}\exp(-\beta\tau_i)][A\ \exp(-a\tau_i) + B\ \exp(-b\tau_i)]$$

Case 4- Prob{LOP*[AFTS*BFTR + AFTR*BFTS]| $t>\tau_i$} =

$$2\lambda_n\lambda_f q_f \exp(-2\alpha\tau_i)\{A[\exp(-a\tau_i)]/[\lambda_f + \alpha + a]$$

$$+ B[\exp(-b\tau_i)]/[\lambda_f + \alpha + b]\}$$

Case 5- Prob{LOP*CCFTR| $t>\tau_i$} =

$$\lambda_n\lambda_c \exp(-\beta\tau_i)\{A[\exp(-a\tau_i)]/[\lambda_c + a] + B[\exp(-b\tau_i)]/[\lambda_c + b]\}$$

Case 6- Prob{LOP*AFTR*BFTR| $t>\tau_i$} =

$$2\lambda_n[\lambda_f^2 \exp(-2\alpha\ )/(\alpha + \lambda_f)]\{A[\exp(-a\tau_i)]/[2\lambda_f + a]$$

$$+ B[\exp(-b\tau_i)]/[2\lambda_f + b]\}$$

To assess the distribution in coping times each of the above expressions must be evaluated for the discrete distributed τ_i values in the following manner:

$$\text{Prob}\{\text{Sequence}\} = \sum \text{Prob}\{\text{Sequence} \mid t > \tau_i\} \times w(\tau_i)$$

where $w(\tau_i)$ is the normalized weighting for each τ_i value. These weights were derived from the discrete probability distribution developed in Section 4.1.5.

Sequence quantification and uncertainty analysis was performed using the SPASM Code (Reference 13). This is a general purpose Monte Carlo system simulation code. It requires as inputs:

- o An algebraic expression in terms of random variables describing the overall result.

- o The mean, variance, and distribution type for each of the random variables to be Monte Carlo sampled.

The SPASM code was used to generate random solutions to the sequence probability expression by randomly sampling each of the key random variables (e.g.: frequency of loss of offsite power, recovery time, diesel failure rate, etc.). Each of the resultant random solutions was then binned by resultant probability until the number of sampled results was large enough to perform statistics (30,000 samples). From the resultant binning process, sample means and variances were then computed along with upper and lower confidence bounds. Appendix C shows the actual computer input/output results and the input variables for the Monte Carlo sampling process.

5. INTERPRETATIONS OF THE RESULTS

This section discusses the results obtained and what inferences can be made on possible areas for improvement.

5.1. Results

The results of the quantification of the total core damage frequency is shown in Figure 18. The numerical results are summarized below.

Case:	$(\lambda)_{.50}$	$<\lambda>$	$(\lambda)_{.95}$
Case 1	1.47×10^{-8}	2.71×10^{-8}	9.55×10^{-8}
Case 2	4.63×10^{-9}	1.19×10^{-8}	4.79×10^{-8}
Case 3	5.36×10^{-7}	8.67×10^{-7}	2.75×10^{-6}
Case 4	6.07×10^{-8}	1.05×10^{-7}	3.46×10^{-7}
Case 5	5.83×10^{-7}	9.49×10^{-7}	3.08×10^{-6}
Case 6	1.52×10^{-7}	5.63×10^{-7}	2.44×10^{-6}
TOTAL	1.91×10^{-6}	2.52×10^{-6}	6.65×10^{-6}

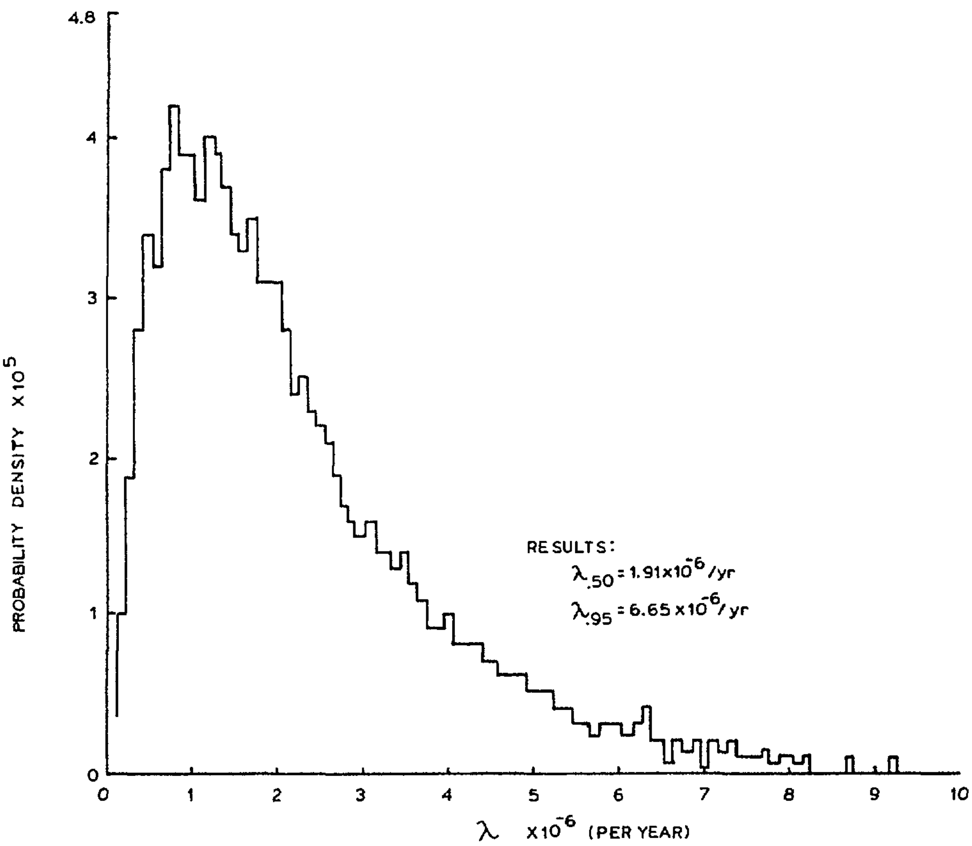


FIG. 18. Millstone Unit 3 station AC blackout core melt frequency Monte Carlo simulation (30 000 samples).

5.2. Interpretations

Figure 19 shows via a bar chart the relative magnitudes of the various core damage contributors. A review of the results of this analysis and the uncertainties yields the following insights:

- The overall median station blackout core damage frequency was found to be 1.9×10^{-6}/yr. The uncertainty analysis considering the following random parameters:

  frequency of loss of offsite power
  restoration time for offsite power
  frequency of diesel maintenance actions
  duration of diesel restoration actions
  demand failure rate of diesels
  running failure rate of diesels
  station battery depletion time

  indicates that this value could be as high as 6.7×10^{-6}/yr due to randomness (an error factor of roughly 3.5).

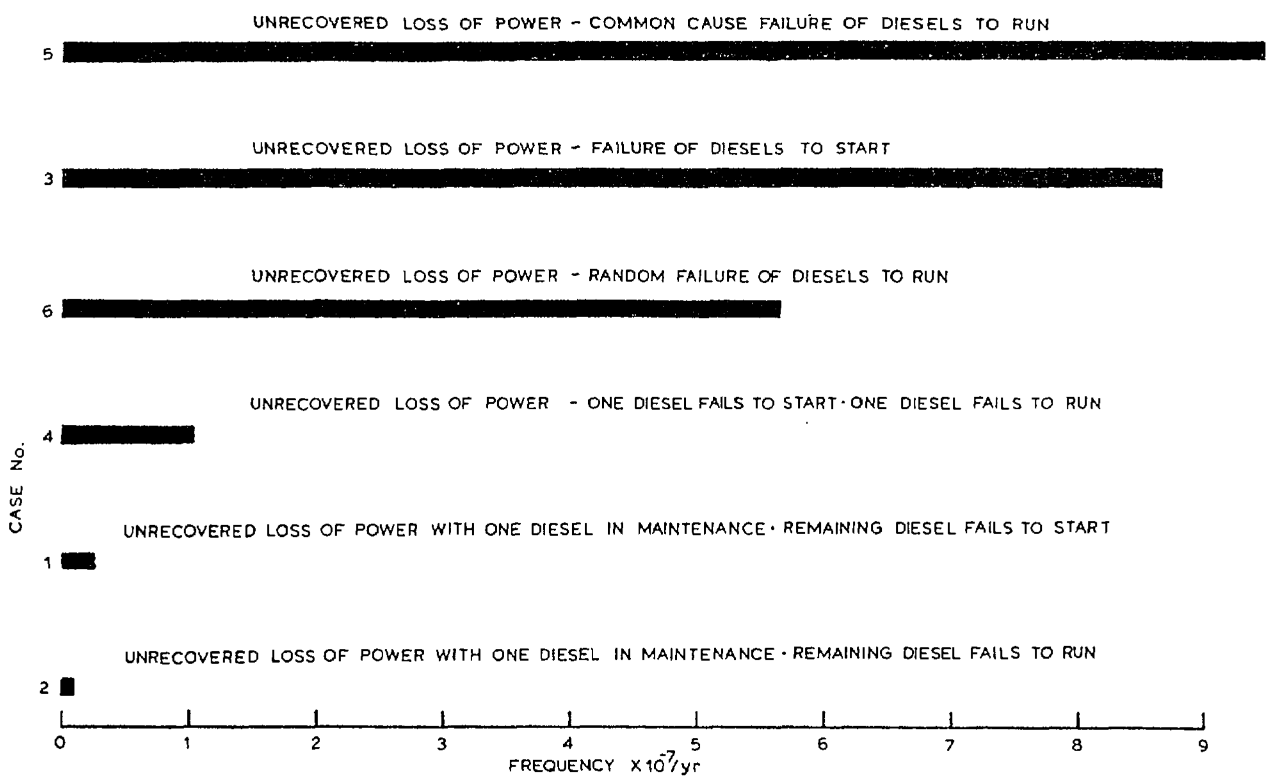


FIG. 19. Comparison of station AC blackout core melt contributors.

- In addition to random uncertainties there are also modeling uncertainties. These in general have been treated with slight conservatisms from what is assumed. As an example use of a 10^{-5} probability for behavior of RCP seals (Appendix A) behaving counter to an established force balance represents the possibility of"non-mechanistic" failures. It must, however, be acknowledged that a model represents primarily what the analyst can mechanistically model. The following modeling uncertainties were addressed with slight conservatisms:

  RCP seal leak rates are assumed to be the same on all 4 RCPs (this is very conservative)
  switchgear room heatup
  steam driven AFW pump compartment heatup

  Because slightly conservative values have been used in these areas it is not likely that these terms could contribute to a higher error factor than that already noted.

- Diesel maintenance unavailability (characterized by the frequency of occurrence and average duration) is by far the least dominant contributor to station blackout core damagefrequency. The total maintenance contribution is only about 1.5%

- Common cause failures on the other hand are predicted to be the most dominant failure mode and make up 72% of the cause of station blackout related core damage.

At the time this analysis was performed, there was a concern on the part of federal regulators that the mean core damage frequency due to station blackout could be as high as 8.2×10^{-5}/yr and represent the bulk of the overall core damage frequency. When coupled with an assessment of the offsite public consequences such a value could justify expenditures of several million dollars to reduce the risk of station blackout. The implications of this analysis indicated the risk was 1/33 of that estimated by federal regulators and that multimillion dollar backfits could not be justified. Federal regulators ultimately concurred with this finding.

Additional insights gained from this analysis include the fact that the mean coping time is roughly 8.78 hours (from Section 4.1.5). In computing this value it is interesting to note that battery failure (due to random or common cause failures) is the most dominant concern in the short term. This is not surprising because of the fact that even if the RCP seals were to all fail at the very start of the event considerable time is required to boil off all of the water on top of the core and in the RCS loops. This is effectively shown in Figure 12. RCP seal failure becomes a more dominant consideration in the 4 - 7 hr time frame. In time periods beyond this, the load discharge capability of the batteries becomes the dominant limitation.

6. PEER REVIEW PROCESS

Following internal technical review within Northeast Utilities this analysis was submitted to the U.S. Nuclear Regulatory Commission in March 1986. The following issues were reviewed in the course of the peer review process:

- the diesel reliability data based on Northeast Utilities experience (Sensitivity studies were requested on the effects of higher diesel failiure on demand rates. The results indicated little effect because of the dominance of common cause failure.)

- the reasonability of the offsite power recovery model (This model includes regional data for momentary to multi-hour power outages. Both loss of offsite power events at Millstone were multi-hour events.)

- the time dependent PSA model (This was found to be similar to work developed in Reference 7 with the exception that the regulators had used a demand maintenance unavailability model, or q_m term instead of frequency of maintenance and mean restoration time.)

In addition to these key items the reviewers also investigated the reasonability of the assumption that no other failures could occur which could compound the situation and thus reduce the coping time as a result of other effects. Control room heatup due to loss of HVAC was considered but this was found to be insignificant. An actual total loss of HVAC event had occurred for a prolonged period of time while at full power operation and without any significant effect on plant personnel or equipment. Loss of control and instrument air was also raised as a possible issue. This was ruled out on the basis that such air was not needed for recovery of the diesels as they each have sufficient compressed air for several start attempts. Loss of air would also have no effect on the steam driven AFW pump because the air operated valves (AOVs) which must open for startup are designed to fail open on loss of air.

Appendix A

REACTOR COOLANT PUMP SEAL LOCA LEAKAGE RATES AND PROBABILITY MODEL

INTRODUCTION

The reactor coolant pump seal leakage rates following a loss of all seal cooling incident is an important parameter in the determination of the consequences of the incident. The leakage rate determines the time between the loss of seal cooling and the time at which core degradation begins. This time interval represents the time during which the recovery of the pump seal cooling will result in a safe stable reactor core state. Recovery of pump seal cooling after initial core degradation begins will result in consequences of varying magnitudes both in terms of utility costs and offsite radiation exposures.

The magnitude and timing of the reactor coolant pump seal leakages following a loss of all cooling incident is dependent upon the performance of a number of components within the seal assembly. The likelihood of various combinations of successes and failures of the individual components determines the likelihood of various leakage rates from the reactor coolant pump seals under a loss of cooling condition. The event tree methodology can facilitate the understanding of the reactor coolant pump seal performance under loss of all cooling conditions. Given a loss of all pump seal cooling, the chronology of events which lead to various levels of pump seal leakage can be displayed as discrete nodal questions on an event tree.

POTENTIAL LEAKAGE RATES

The potential leakage rates from the reactor coolant pump seals under a loss of all cooling conditions can be divided into three phases: the pump seal transient heatup period, the quasi-steady state period during which the pump seals are in thermal equilibrium with the reactor coolant fluid, and the long term period in which depressurization of the reactor coolant system occurs either by inventory loss through the pump seal or operator actions to depressurize the reactor coolant system.

During the heatup phase, the reactor coolant pump seal assembly undergoes thermally induced phenomena which results in a change in the leakage rate compared to the normal operational condition. This phase begins at approximately 10 minutes following the loss of seal cooling as the thermal barrier heat exchanger loses it's thermal capacitance and the cold fluid in the seal inlet cavity is purged. The seal components (e.g. #1 seal faces, ring, runner, etc.) are expected to become thermally saturated within 30 minutes of the loss of seal cooling.

During this phase, the #1 seal runner will move axially on the pump shaft to accommodate differences in thermal growth of the pump components. The movement is governed by the force balance across the #1 seal assembly. Thus, the #1 seal is expected to remain functional and control the leakage to relatively low values. However, should the #1 seal runner experience extreme binding on the pump shaft due to postulated premature thermally induced extrusion failure of the O-rings or channel seals, the #1 seal faces would tend to open as a result of the difference in thermal growth motion between the reactor coolant pump shaft and seal housing. This would result in an increased leakage rate across the #1 seal assembly. This is a VERY low probability event as it requires very rapid degradation of the O-ring material during the early part of the heatup transient which is not supported by test results.

At this point, the #2 seal is expected to briefly enter a film-riding mode of operation, due to the increased pressure in the area between the #1 and #2 seal assemblies. However, the #2 seal runner, which is also free to move axially on the pump shaft is expected to quickly rotate to a closed position as a result of restoring forces on the runner, thus limiting overall leakage across the reactor coolant pump seal assembly. However, if thermally induced premature O-ring extrusion failures occur in the #2 seal area, the runner may not be able to rotate to a closed position due to binding on the shaft, thereby remaining in an open position. This is a very low probability event as it requires very rapid degradation of the O-ring material during the early part of the heatup transient which is not supported by the test results.

The leakage rate across the #1 seal assembly is expected to increase from the normal rate of 12 gpm per pump to a value on the order of 60 gpm during the early part of the thermal transient and then quickly fall to a quasi-equilibrium rate of approximately 21 gpm. The half-width of the leakage rate spike is expected to be approximately two minutes. In the worst case, where the #1 seal runner binds such that the #1 seal is opened to the maximum limit of its travel, and the #2 seal remains in the full open position, the leakage rate is estimated to be 480 gpm. However, for the case of a full open #1 seal with the #2 seal returning to a closed position, the leakage rate is expected to be approximately 75 gpm. These leakage rates are postulated to occur at approximately 30 minutes after the loss of all seal cooling.

During the equilibrium phase of the transient, the reactor coolant pump seal assembly is in thermal equilibrium with the reactor coolant system. This phase extends from the end of the heatup period to at least 2 hours following the loss of all seal cooling. During this phase of the transient the #1 seal is expected to remain functional with low leakage rates of 21 gpm or less which would slowly decrease with the slow, natural depressurization of the reactor coolant system. During this phase, O-ring material which is not specifically qualified for high temperature performance may begin to deteriorate. Test data indicates that no significant extrusion results for at least two hours after the loss of all seal cooling. Should O-ring extrusion occur which results in the deterioration of the O-ring integrity, the leakage rates may increase. If the O-ring deterioration involves one of the few 'critical' O-rings in the #1 seal assembly, the leakage might increase to approximately 60 gpm. This is also a low probability event. Tests involving O-ring material shows that O-ring integrity degradation, while not expected to occur, is possible due to the high temperatures. Given a degradation of the O-ring material, the #2 seal is expected to remain in the rubbing-face mode which limits the leakage to 60 gpm. However, if the #2 seal goes into a film-riding mode due to the pressure differential across the seal and degradation of 'critical' O-rings in the #2 seal assembly, the leakage rate could increase to approximately 175 gpm.

During the depressurization phase, the reduction in reactor coolant system pressure has a direct impact on the pump seal leak rates. Reactor coolant system depressurization is certain if the leakage rate is large, due to the inventory loss from the reactor coolant system. However, if the leakage rate is small, reductions in reactor coolant system pressure, to the accumulator setpoint, would only occur if direct operator action is taken to depressurize the secondary side of the steam generators. The expected leakage rate through the pump seals would be reduced to approximately 10 gpm for a reactor coolant system pressure of 600 psig.

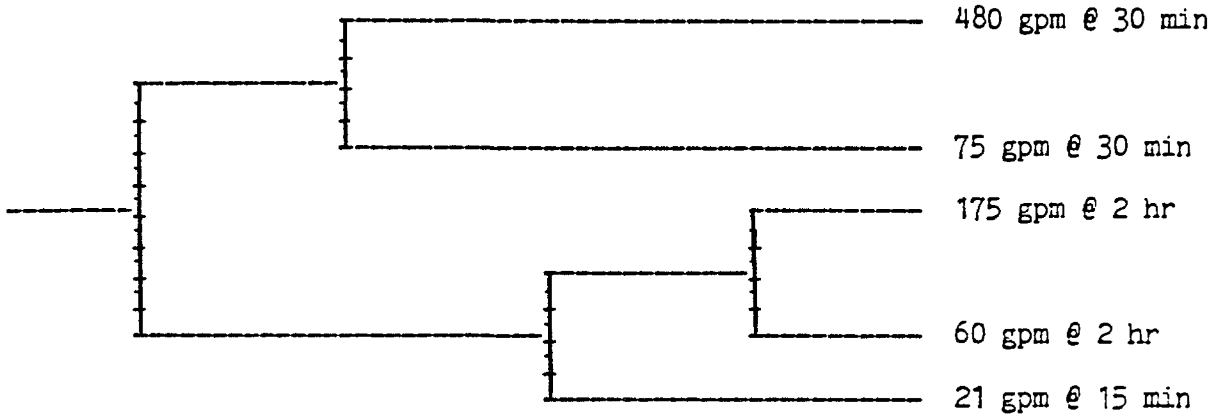
PUMP LEAKAGE EVENT TREE

Based on the phenomena involved in degradation of the reactor coolant pump seal assembly following a complete loss of cooling, an event tree can be constructed which displays these phenomena on a logical format. The nodal questions on the event tree would represent the important phenomena that determine the long term leakage rates from the seal assembly and the end-points of the tree would identify the long term leakage rate for a given path in the event tree.

A highly simplified event tree for the leakage from the seal assembly following a complete loss of cooling can be displayed as follows:

REACTOR COOLANT PUMP SEAL LEAKAGE
FOLLOWING A COMPLETE LOSS OF SEAL COOLING

Does #1 Seal Bind Open ?	Does #2 Seal Open ?	Do O-rings Degrade ?	Does #2 seal Open ?	Resultant RCP Leakage Rate



TIME TO CORE UNCOVERY

The time between the initiation of the loss of all seal cooling and the time to core uncovery is an indicator of the time frame available for the recovery of pumped safety injection to maintain the core in an undamaged condition. Based on analyses of the loss of all a.c. power using the WFLASH computer code, this time frame can be estimated assuming no operator action to depressurize the reactor coolant system by reducing the secondary system pressure. The analyses assume that the turbine driven auxiliary feedwater pump remains available to remove decay heat from the reactor coolant system following the loss of all a.c. power. The time period between the initiation of the event and the time of core uncovery for the various leakage rates presented above are:

480 gpm	1.5 hr.
175 gpm	5.5 hr.
75 gpm	7.5 hr.
60 gpm	11 hr.
21 gpm	15 hr.

These times take into account the time lag between the initiation of the event and the time at which the leakage increases to the steady state value at reactor coolant system operating pressures given above.

Analyses are also available to estimate the time interval between the event initiation and the beginning of core uncovery (WFLASH code) for cases with operator action to cool the reactor coolant system at a rate of 100 degrees per hour using the turbine driven auxiliary feedwater pump and the secondary side relief valves. The time available for this case are estimated to be:

480 gpm	2 hr.
175 gpm	7 hr.
75 gpm	11 hr.
60 gpm	15 hr.
21 gpm	>20 hr.

EXAMPLE LEAKAGE PROBABILITY

In order to illustrate the significance of the event tree approach to the pump seal LOCA issue, an example quantification of the tree is performed and a discussion of the results is given. The example below relies upon the assignment of best estimate split fractions for the nodal questions in the event tree given above. These probabilities are based on the experimental data and analyses performed for the Westinghouse Owner's Group (WOG) Pump Seal LOCA Program.

The probability of the #1 seal binding in an open is assigned a value of 0.01% based on the events which must occur during the heatup phase. The assignment of a 1% value reflects the judgement that the events necessary to cause seal binding are very unlikely, based on experimental evidence, analyses of the binding forces and restoring forces, and engineering judgement. In order for this magnitude of leakage to occur, the O-ring degradation must very rapidly occur during the early part of the heatup phase, which is not supported by any of the O-ring test data. For this exercise, any binding of the #1 seal is conservatively assumed to result in the #1 seal binding in the full open position, thus resulting in the maximum leakage rate.

For any condition in which the pressure in the area between the #1 and #2 seals is high, the probability of the #2 seal going into the film-riding mode is assigned a value of 1%. In the case of excessive leakage past the #1 seal, the pressure in the area between the #1 and #2 seals will be high. Unless there is significant degradation of the #2 seal O-rings during this early phase,the #2 seal is expected to close to the rubbing face mode of operation, even under high pressures, based on analyses of the opening and closing forces on the #2 seal. Thus the assignment of a value of 1% is very conservative.

The probability of O-ring extrusion in the long term is conservatively estimated to be a uniform rate function over the period of 2 to 12 hours, with a 100% probability by 12 hours. The probability that the O-ring extrusion will lead to O-ring deterioration in the critical O-rings in the #1 seal area is conservatively assigned a value of 50% at two hours. No credit is taken in this sample evaluation for the time dependence of O-ring failures. This is judged to be a conservative estimate of the degradation probability, based on the experimental and analytical results of the O-ring testing program.

Given that the O-rings extrude in the long term following the loss of all seal cooling and result in O-ring failures, the performance of the #2 seal will have a direct impact on the seal assembly leakage rate. In this case, the long term degradation of the O-rings in the #2 seal assembly are treated in an identical manner to those of the #1 seal. Thus, a 50% probability of failure of the #2 seal to remain in the rubbing face mode of operation is assigned to this node, based on O-ring performance.

The expected leakage rate for the case of O-ring degradation in the #1 seal assembly with the #2 seal remaining in its normal film-riding mode would be approximately 60 gpm. The more remote case of failures of critical O-rings in both the #1 and #2 seal assemblies would lead to a seal assembly leakage rate on the order of 175 gpm.

The results of the pump seal assembly leakage, based on these conservative estimates of probability are:

480 gpm	0.00001
175 gpm	0.249975
75 gpm	0000099
60 gpm	0.249975
21 gpm	0.49995

SENSITIVITY TO PROBABILITIES ASSIGNED

The sensitivity of the results of this sample quantification were examined to determine the critical probability assignments. The results of this evaluation show that even if the probabilities on the catastrophic #1 seal leakage are increased by two orders of magnitude to 1% and the probability of the #2 seal going in to the film-riding mode is increased to 50%, the results are virtually unchanged. The evaluation further shows that only significant changes to the catastrophic seal leakage rate probabilities to near unity will have a significant impact on the results.

RESULTS OF EXAMPLE QUANTIFICATION

The probabilistic distribution of pump seal leakage rates developed in the above example have a significant impact on the core melt and risk dominance of the complete loss of reactor coolant pump seal cooling. A rough estimate of the impact of this approach on the core melt probability attributed to the station blackout can be obtained from a simplistic treatment of the NRC power recovery model for the Millstone Unit 3 station.

Using NRC analysis values for Millstone Unit 3 for loss of offsite power and loss of onsite a.c.power (NUREG-1152 Draft), the probability of core melt using the 'old' pump seal LOCA model of 300 gpm per pump at 30 minutes after loss of a.c.power was estimated to be 1 E(-4). Using the above values and assuming no operator action to depressurize the reactor coolant system, the core melt frequency would be reduced to the neighborhood of 2.5 E(-5). It should be noted that this reduction by a factor of 4 is based on some very conservative estimates of the loss of offsite power for extended time periods. The minimum core melt frequency to be obtained for the expected pump seal performance (i.e 21 gpm per pump leakage rate) with a probability of 1.0 would be in the neighborhood of 1.6 E(-5). Thus, the reduction in this case is still significant.

CONCLUSIONS

The development of the knowledge of pump seal leakage under conditions of all loss of cooling has progressed to a point where we understand the mechanisms leading to large leakage rates (e.g. pump seal LOCA) and have limited experimental data on the behavior of the pump seals under realistic conditions. Further, the loss of all a.c. power event leading to a large pump seal LOCA is generally a dominant contributor to core melt frequency and to risk in risk assessment studies. The dominance of this event in risk assessment space is largely due to the treatment of the magnitude and timing of the pump seal LOCA (i.e. a 300 gpm per pump leak beginning at 30 minutes). Using this model, the time available for recovery prior to the beginning of core degradation is approximately 2 hours.

Using an approach as outlined above, the pump seal leakage following a complete loss of cooling is treated as an event which could result in a range of leakage rates in which the probabilistic distribution is skewed toward the lower leakage rates (the expected case as supported by limited test data). Using this approach, the time available for recovery prior to core degradation increases significantly.

The event tree approach and quantification for treating the pump seal leakage under loss of all cooling conditions, for which preliminary development was outlined above, is based on a conservative treatment of the pump seal performance under loss of all cooling conditions. This conservative treatment, which can easily be defended results in about 75% of the potential credit that could be obtained for pump seal integrity. A stronger case for pump seal integrity credit would require additional effort to: (1) include all important mechanisms related to the magnitude and timing of pump seal leakage and (2) the development of a less conservative basis for the split fractions related to these important mechanisms based on available test data, analyses and engineering judgement.

Appendix B

CALCULATION OF MILLSTONE SITE LOSS OF OFFSITE POWER FREQUENCY

The purpose of this analysis is to calculate the frequency of loss of offsite power at the Millstone Nuclear Power Station site. The frequency of this event is assumed to be Gamma distributed. The Gamma distribution is an appropriate model as it provides the time required for exactly "η" events to take place if events occur at a constant rate "λ". The failure rate of loss of offsite power is calculated by using Bayesian estimation techniques which utilize the loss of offsite power failure rate history at the Millstone site. The prior failure rate for the Bayesian update was calculated by the maximum likelihood estimation method. For this, the failure data of plants in the Northeastern Power Coordinating Council (NPCC) region is used.

The table on the following page summarizes the existing regional loss of offsite power experience through 1984.

NPCC REGIONAL LOSS OF OFFSITE POWER EXPERIENCE THROUGH 1984

Nuclear Plant Site	No. of Events	Hours	Point Estimate λ_{ni}
Fitzpatrick	1	79716	1.25×10^{-5}/hr
Robert E. Ginna	2	122640	1.63×10^{-5}/hr
Conn. Yankee	5	142788	3.50×10^{-5}/hr
Indian Point	4	185712	2.15×10^{-5}/hr
Maine Yankee	3	97236	3.08×10^{-5}/hr
Millstone	1	118260	8.46×10^{-6}/hr
Nine Mile Point	2	125268	1.59×10^{-6}/hr
Pilgrim	2	100740	1.98×10^{-5}/hr
Vermont Yankee	1	103368	9.67×10^{-6}/hr
Yankee Rowe	1	203232	4.92×10^{-6}/hr

The maximum likelihood estimation of the prior distribution proceeds with the following assumptions. Suppose one has "N" samples of an hourly failure rate. The samples are collected by observing a Poisson process until N failures have occurred and thus N estimates of "λ_n" exist. Each observation yields a point estimate of "λ_n", denoted λ_{ni} which is assumed to be a sample from a Gamma distribution characterized by the following density function:

$$f(\lambda_n|\alpha,\beta) = \frac{\alpha}{\Gamma(\beta)} (\alpha\lambda_n)^{\beta-1} e^{-\alpha\lambda_n}$$

-where α, β are unknown.

The likelihood function is defined:

$$\mathcal{L}(\alpha,\beta) = \prod_{i=1}^{N} f(\lambda_{n_i}|\alpha,\beta)$$

The maximum likelihood estimators for α, β may be obtained as the solution to the following system of equations:

(i) $$\frac{\partial}{\partial\alpha} \ln \mathcal{L}(\alpha,\beta) = 0$$

(ii) $$\frac{\partial}{\partial\beta} \ln \mathcal{L}(\alpha,\beta) = 0$$

Taking the first expression:

$$0 = \frac{\partial}{\partial\alpha} \ln \left[\prod_{i=1}^{N} f(\lambda_{n_i}|\alpha,\beta) \right]$$

Substituting in for $f(\lambda_{n_i}|\alpha,\beta)$ yields the following:

$$0 = \frac{\partial}{\partial\alpha}\sum_{i=1}^{N}\left[\ln\alpha - \ln\Gamma(\beta) + (\beta-1)\ln\alpha + (\beta-1)\ln\lambda_{n_i} - \alpha\lambda_{n_i}\right]$$

$$= \sum_{i=1}^{N}\left[\frac{1}{\alpha} + \frac{\beta-1}{\alpha} - \lambda_{n_i}\right]$$

$$= \sum_{i=1}^{N}\left[\beta - \alpha\lambda_{n_i}\right]$$

$$= N\beta - \sum_{i=1}^{N}\alpha\lambda_{n_i}$$

$$N\beta = \alpha\sum_{i=1}^{N}\lambda_{n_i}$$

Solving for α yields:

$$\alpha = \frac{\beta}{\frac{1}{N}\sum_{i=1}^{N}\lambda_{n_i}} = \frac{\beta}{\langle\lambda_n\rangle}$$

Thus: $\alpha = \beta/\langle\lambda_n\rangle$

Where $\langle\lambda_n\rangle$ is the sample mean failure rate obtained from the N samples. Now taking the second equation:

$$0 = \frac{\partial}{\partial\beta}\ln\left[\prod_{i=1}^{N} f(\lambda_{n_i}|\alpha,\beta)\right]$$

$$= \frac{\partial}{\partial\beta}\sum_{i=1}^{N}\left[\ln\alpha - \ln\Gamma(\beta) + (\beta-1)\ln\alpha + (\beta-1)\ln\lambda_{n_i}\right]$$

$$= \sum_{i=1}^{N} -\frac{\partial}{\partial\beta}\ln\Gamma(\beta) + \ln\alpha\lambda_{n_i}$$

$$= -N\frac{\partial}{\partial\beta}\ln\Gamma(\beta) + \sum_{i=1}^{N}\alpha\lambda_{n_i}$$

$$N \frac{\partial}{\partial \beta} \ln \Gamma(\beta) = \sum_{i=1}^{N} \ln \alpha \lambda_{n_i}$$

$$\exp\left[N \frac{\partial}{\partial \beta} \ln \Gamma(\beta)\right] = \prod_{i=1}^{N} \alpha \lambda_{n_i} = \alpha^N \prod_{i=1}^{N} \lambda_{n_i}$$

Solving for α^N yields:

$$\alpha^N = \frac{\exp\left[N \frac{\partial}{\partial \beta} \ln \Gamma(\beta)\right]}{\prod_{i=1}^{N} \lambda_{n_i}}$$

$$\alpha = \left[\frac{\exp\left[N \frac{\partial}{\partial \beta} \ln \Gamma(\beta)\right]}{\prod_{i=1}^{N} \lambda_{n_i}}\right]^{1/N}$$

Thus:

Noting that: $\psi(\beta) = \frac{\partial}{\partial \beta} \ln \beta!$ and that: $\beta! = \Gamma(\beta+1) = \beta \Gamma(\beta)$

$$\psi(\beta) = \frac{\partial}{\partial \beta} \ln \beta! = \frac{\partial}{\partial \beta} \ln \beta \Gamma(\beta) = \frac{1}{\beta} + \frac{\partial}{\partial \beta} \ln \Gamma(\beta)$$

$$\frac{\partial}{\partial \beta} \ln \Gamma(\beta) = \psi(\beta) - \frac{1}{\beta}$$

Substituting this expression in to the equation for α yields:

$$\alpha = \left[\frac{\exp\left[N\left(\psi(\beta) - \frac{1}{\beta}\right)\right]}{\prod_{i=1}^{N} \lambda_{n_i}}\right]^{1/N}$$

There are now two equations in two unknowns:

(i) $\alpha = \beta / \langle \lambda_n \rangle$

(ii) $$\alpha = \left[\frac{\exp\left[N\left(\Psi(\beta) - \frac{1}{\beta}\right)\right]}{\prod_{i=1}^{N} \lambda_{n_i}} \right]^{1/N}$$

The solution of these equations either via graphical or numerical techniques defines unique values for α and β. From the industry data for the NPCC region, N = 10. The mean industry failure rate is 1.53×10^{-1}/yr. Numerical solution yields:

$$\alpha = 22.3$$

$$\beta = 3.43$$

The mean and variance of the prior distribution would thus be:

$$\langle \lambda_n \rangle = \int_0^{+\infty} f(\lambda_n | \alpha,\beta)\, \lambda_n \, d\lambda_n = \int_0^{+\infty} \frac{\alpha \lambda_n}{\Gamma(\beta)} (\alpha\lambda_n)^{\beta-1} e^{-\alpha\lambda_n} \, d\lambda_n$$

$$= \frac{\beta}{\alpha} = \frac{3.43}{22.3} = 1.538 \times 10^{-1}/yr$$

$$\mathrm{Var}\, \lambda_n = \int_0^{+\infty} f(\lambda_n | \alpha,\beta)\, (\lambda_n - \langle \lambda_n \rangle)^2 \, d\lambda_n = \frac{\beta}{\alpha^2}$$

$$= \frac{3.43}{(22.3)^2} = 6.89 \times 10^{-3}/yr^2$$

Applying Bayes theorem, the posterior probability density function for each initiating event in which "f" events have been observed in "T" years can be expressed as follows:

$$F_{\lambda_n}(\lambda_n | f, T, \alpha, \beta) = \frac{(\alpha+T)^{\beta+f}}{\Gamma(\beta+f)} \lambda_n^{\beta+f-1} e^{-(\alpha+T)\lambda_n}$$

Using this expression, the updated mean and variance can be expressed as follows based on 2 events in 15 years:

$$\langle \lambda_n \rangle' = \frac{\beta + f}{\alpha + T} = \frac{3.43 + 2}{22.3 + 15} = 1.45 \times 10^{-1}/\text{yr.}$$

$$\text{Var } \lambda_n' = \frac{\beta + f}{(\alpha + T)^2} = \frac{3.43 + 2}{(22.3 + 15)^2} = 3.9 \times 10^{-3}/\text{yr.}^2$$

Appendix C

SPASM COMPUTER ANALYSIS OF TOTAL CORE DAMAGE FREQUENCY

Data Used in the Core Melt Frequency Model

Term	Mean Value	Variance	Distribution	Data Source
λ_n	1.45×10^{-1}/yr	3.92×10^{-3}	Gamma	MP-1 PSS Updated for Hurricane Gloria
q_f	6.7×10^{-3}	9.6×10^{-6}	Beta	MP-1 PSS
q_{cc}	2.59×10^{-4}	9.0×10^{-8}	Gamma	NUREG/CR-2099
λ_f	1.1×10^{-3}/hr	1.1×10^{-6}	Gamma	MP-1 PSS
λ_{cc}	9.0×10^{-5}/hr	8.1×10^{-9}	Gamma	NUREG-1152
λ_m	5.25×10^{-5}/hr	2.76×10^{-9}	Gamma	MP-1 PSS

The non-restoration distributions are given by the following expressions:

Offsite Power: $Q_n(t) = A \exp(-at) + B \exp(-bt)$

$A = 0.4 \qquad a = 0.297$

$B = 0.6 \qquad b = 4.6$

Emergency Diesel: $Q_f(t) = \exp(-t/15)$ (based on NUREG-1152)

Diesel Maintenance: $Q_m(t) = \exp(-t/15)$ (based on NUREG-1152)

Common Cause: $Q_c(t) = \exp(-t/10)$ (based on NUREG-1152)

FUNCTION SYSTEM FOR NSPASM2 CODE NWAMCUT/NSPASM VERSION 2

```
      DOUBLE PRECISION FUNCTION SAMPLE(X,IFLAG,NPROB)
      DIMENSION T(6),W(6),X(1),Z(7)
      DOUBLE PRECISION T,W,X,Z
      DATA T/1.5D+0,4.0D+0,5.5D+0,8.0D+0,1.2D+1,1.5D+1/
      DATA W/4.30D-5,5.00D-2,2.37D-1,3.38D-1,3.50D-1,2.50D-2/
      SAMPLE=0.0D+0
      DO 10 I=1,6
      Z(1)=DEXP(-T(I)/1.0D+1)
      Z(2)=DEXP(-2.0D+0*T(I)/1.5D+1)
      Z(3)=4.0D-1*DEXP(-2.97D-1*T(I))
      Z(4)=6.0D-1*DEXP(-4.60D+0*T(I))
      Z(5)=1.0D+0/1.5D+1
      Z(6)=2.97D-1
      Z(7)=4.60D+0
      SAMP1=X(6)*X(6)*Z(2)+X(5)*Z(1)
      SAMP1=SAMP1+(2.0D+0*X(3)*X(6)*Z(2))/(X(4)+Z(5))
      SAMP1=SAMP1*X(4)*(Z(3)+Z(4))
      SAMP2=(Z(3)/(X(2)+Z(5)+Z(6)))+(Z(4)/(X(2)+Z(5)+Z(7)))
      SAMP2=SAMP2*(2.0D+0*X(2)*X(3)*X(4)*Z(2))/(X(2)+X(4)+Z(5))
      SAMP3=(Z(3)/(X(2)+Z(5)+Z(6)))+(Z(4)/(X(2)+Z(5)+Z(7)))
      SAMP3=SAMP3*2.0D+0*X(2)*X(4)*X(6)*Z(2)
      SAMP4=(Z(3)/(X(1)+Z(6)))+(Z(4)/(X(1)+Z(7)))
      SAMP4=SAMP4*X(1)*X(4)*Z(1)
      SAMP5=(Z(3)/(2.0D+0*X(2)+Z(6)))+(Z(4)/(2.0D+0*X(2)+Z(7)))
      SAMP5=SAMP5*(2.0D+0*X(2)*X(2)*X(4)*Z(2))/(X(2)+Z(5))
      SAMPT=SAMP1+SAMP2+SAMP3+SAMP4+SAMP5
   10 SAMPLE=SAMPLE+(W(I)*SAMPT*8.760D+3)
      RETURN
      END
```

Total Core Melt Frequency Due to Station Blackout - SPASM Input/Output

---

FUNCTION SAMPLE FOR NSPASM2 CODE NWAMCUT/NSPASM VERSION 2

```
      DOUBLE PRECISION FUNCTION SYSTEM(Y,IFLAG,NSYS)
      DIMENSION Y(30)
      DOUBLE PRECISION Y
      SYSTEM=Y(1)
      RETURN
      END
```

MP3 LOSP CALCULATIONS DATA

NWAMCUT/NSPASM VERSION 2

INPUT COMPONENT DESCRIPTION

(1) COMPONENT NAME
(2) COMPONENT NUMBER
(3) COMPONENT FAILURE DISTRIBUTION
(4) MEDIAN OF COMPONENT UNAVAILABILITY
(5) ERROR FACTOR OF COMPONENT UNAVAILABILITY

	(1)	(2)	(3)	(4)	(5)
COMPONENT	LAM-SUBC	X(1)	GAMMA	9.0000D-05	8.1000D-09
COMPONENT	LAM-SUBF	X(2)	GAMMA	1.1200D-03	1.0800D-06
COMPONENT	LAM-SUBM	X(3)	GAMMA	5.2500D-05	2.7600D-09
COMPONENT	LAM-SUBN	X(4)	GAMMA	1.6600D-05	5.1100D-11
COMPONENT	LCQ-SUBC	X(5)	BETA	2.2900D-04	6.9300D-08
COMPONENT	LCQ-SUBF	X(6)	BETA	6.7100D-03	9.5600D-06

XEQ TIME = 123.410 SECS

OUTPUT EVALUATIONS, SAMPLE SIZE = 30000

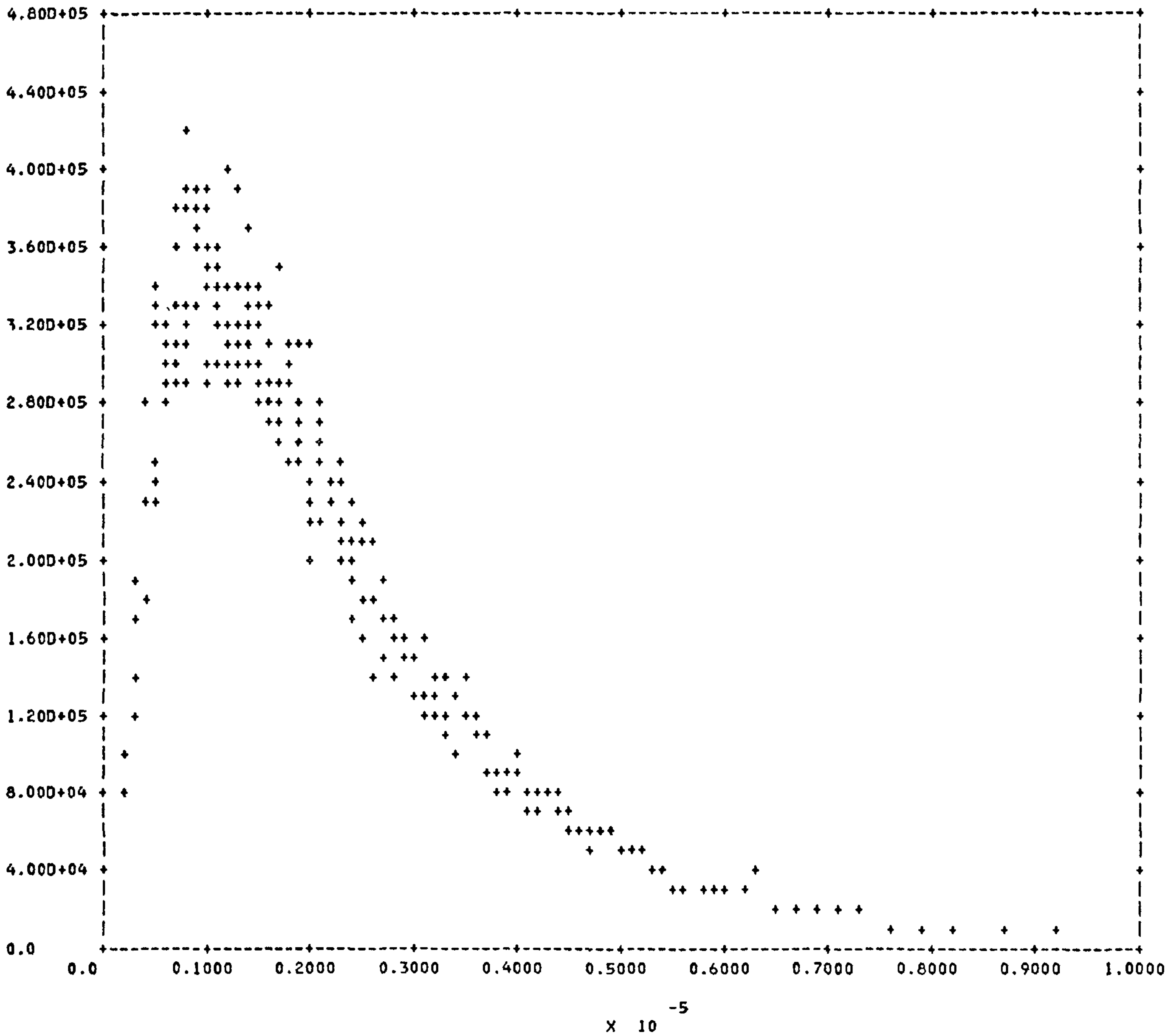
DISTRIBUTION PARAMETERS: MEAN = 2.5236D-06 STANDARD DEVIATION = 2.1962D-06
MEDIAN = 1.9129D-06 MODE = 7.8670D-07
BETA1 = 7.3949D+00 BETA2 = 1.7228D+01

DISTRIBUTION CONFIDENCE LIMITS			95 PER CENT CONFIDENCE BOUNDS ON SIMULATION C.D.F.	
CONFIDENCE (PER CENT)	P.D.F. VALUE	C.D.F. VALUE	UPPER	LOWER
0.5	7.9225D+04	1.6573D-07	2.0251D-07	1.3575D-07
1.0	1.0166D+05	2.2326D-07	2.5965D-07	1.9209D-07
2.5	1.7040D+05	3.3334D-07	3.6899D-07	3.0127D-07
5.0	2.3784D+05	4.5469D-07	4.8951D-07	4.2247D-07
10.0	3.0053D+05	6.3255D-07	6.6617D-07	6.0075D-07
20.0	3.3235D+05	9.3232D-07	9.6392D-07	9.0187D-07
25.0	3.2215D+05	1.0804D-06	1.1110D-06	1.0508D-06
30.0	4.0026D+05	1.2383D-06	1.2679D-06	1.2096D-06
40.0	2.8249D+05	1.5524D-06	1.5799D-06	1.5256D-06
50.0	3.0898D+05	1.9129D-06	1.9379D-06	1.8883D-06
60.0	2.5033D+05	2.3304D-06	2.3525D-06	2.3084D-06
70.0	1.4741D+05	2.9016D-06	2.9250D-06	2.8784D-06
75.0	1.2412D+05	3.2677D-06	3.2936D-06	3.2421D-06
80.0	1.1172D+05	3.6891D-06	3.7178D-06	3.6607D-06
90.0	5.1116D+04	5.0846D-06	5.1228D-06	5.0469D-06
95.0	2.1710D+04	6.6467D-06	6.6954D-06	6.5987D-06
97.5	8.1016D+03	8.3255D-06	8.3855D-06	8.2663D-06
99.0	3.8480D+03	1.0672D-05	1.0748D-05	1.0597D-05
99.5	2.1511D+02	1.2811D-05	1.2902D-05	1.2722D-05

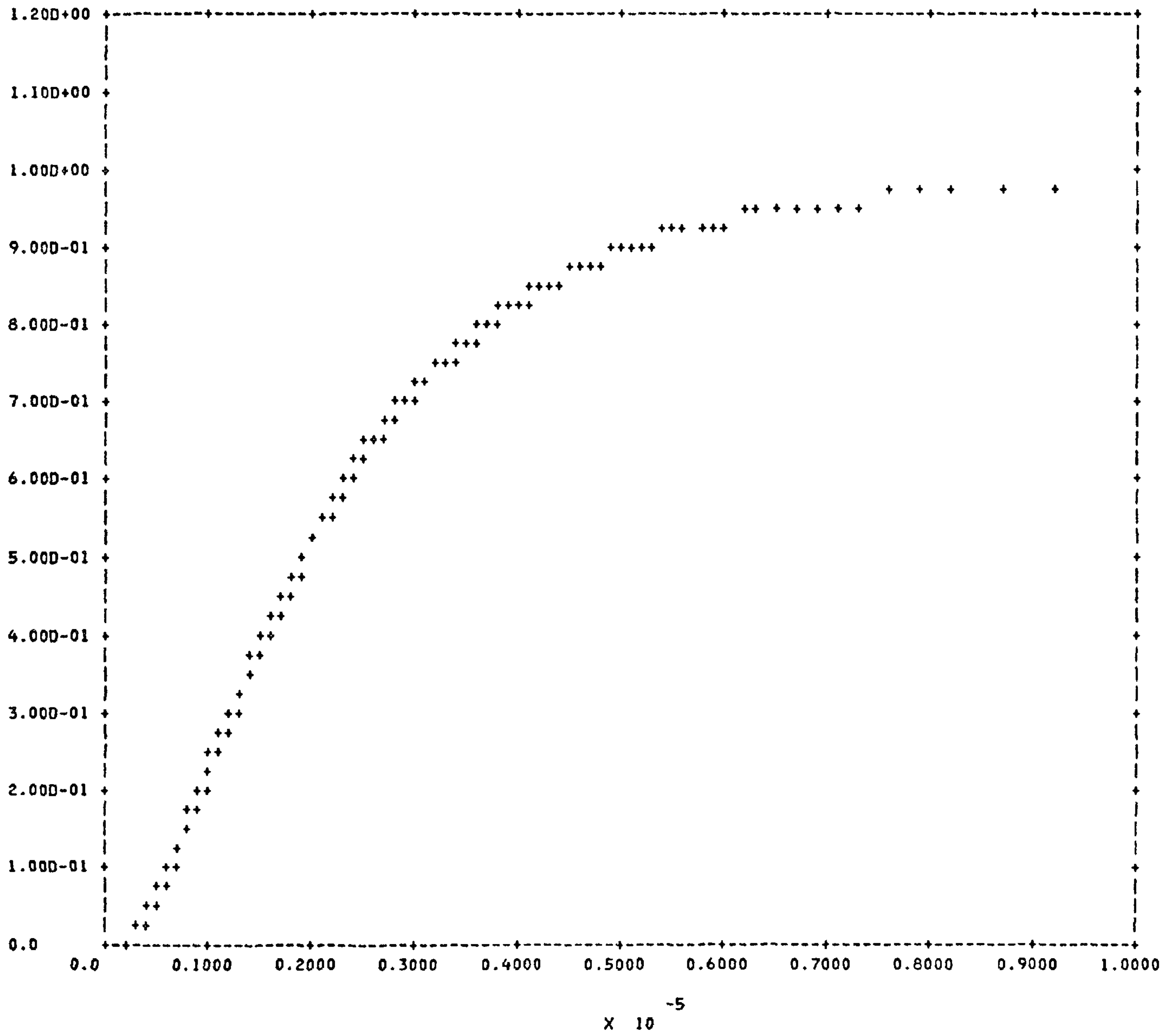
95 PER CENT CONFIDENCE BOUNDS
ON SIMULATION MOMENTS

	UPPER	LOWER
MEAN	2.5444D-06	2.5029D-06
STANDARD DEVIATION	2.2111D-06	2.1817D-06

PROB. DENSITY FUNCTION MEAN = 2.5236D-06 MEDIAN = 1.9129D-06 MODE = 7.8670D-07 STANDARD DEVIATION = 2.1962D-06



CUMULATIVE PROBABILITY MEAN = 2.5236D-06 MEDIAN = 1.9129D-06 MODE = 7.8670D-07 STANDARD DEVIATION = 2.1962D-06



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CONTRIBUTORS TO DRAFTING AND REVIEW

J.H. Bickel
INEL-ETNT Idaho
P.O. Box 1625
Idaho Falls, Idaho 83415
USA

<u>Oversight Committee for the development of a series of PSA case studies</u>

A. Carnino
Electricité de France
32, Rue de Monceau
75384 Paris Cedex 08
France

J. Gaertner
Electric Power Research Institute
Palo Alto, California 94303
USA

S. Hall
Safety & Reliability Directorate
UKAEA
Culcheth, Warrington WA3 4NE
UK

P. Kafka
Gesellschaft f. Reaktorsicherheit (GRS) mbH
Forschungsgelände
8046 Garching
Germany

J. Villadoniga
Consejo de Seguridad Nuclear
S/Sor Angela de la Cruz 3
28020 Madrid
Spain

OECD/NEA
J. Caisely
Nuclear Energy Agency
OECD/NEA
Paris
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