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# Thirteenth Water Reactor Safety Research Information Meeting

Volume 1
- Plenary Session
- Risk Analysis
- Severe Accident Sequence Analysis
- Industry Safety Research

Held at
National Bureau of Standards
Gaithersburg, Maryland
October 22-25, 1985

**U.S. Nuclear Regulatory Commission**

**Office of Nuclear Regulatory Research**

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## ABSTRACT

This six-volume report contains 151 papers out of the 178 that were presented at the Thirteenth Water Reactor Safety Research Information Meeting held at the National Bureau of Standards, Gaithersburg, Maryland, during the week of October 22-25, 1985. The papers are printed in the order of their presentation in each session and describe progress and results of programs in nuclear safety research conducted in this country and abroad. Foreign participation in the meeting included thirty-one different papers presented by researchers from Japan, Canada and eight European countries. The titles of the papers and the names of the authors have been updated and may differ from those that appeared in the final program of the meeting.

PROCEEDINGS OF THE
THIRTEENTH WATER REACTOR SAFETY RESEARCH
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PROCEEDINGS OF THE
THIRTEENTH WATER REACTOR SAFETY RESEARCH
INFORMATION MEETING

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# TRENDS IN NUCLEAR SAFETY RESEARCH - LOOKING AHEAD TO THE 1990'S

Robert B. Minogue, Director
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U. S. Nuclear Regulatory Commission
Washington, DC 20555

Presented at
Thirteenth Water Reactor Safety Research Information Meeting
October 22, 1985

Good morning. I would like to welcome you to the 13th Annual Water Reactor Safety Meeting. For the past 5 years I have had the pleasure of welcoming the participants to this meeting, and I look forward to it every year. On behalf of the NRC, I would like to thank you for coming. I hope the many of you who attended the TMI-2 Core Examination Meeting yesterday found it fruitful. The large attendance at this meeting each year from within the United States and from other countries has contributed significantly to the evolution of this meeting into a broad scientific exchange on issues important to safety. The number of topics has increased and more and more speakers from organizations other than NRC and its contractors are making presentations. By providing this opportunity for broad-based dialogue, we get the benefit of the ideas and scientific understanding of many researchers.

I am very pleased that we have Señor Eduardo González Gómez, who is Commissioner of the Spanish Nuclear Safety Commission (Consejero del Consejo de Seguridad Nuclear) to speak about his country's perspective on nuclear safety and I welcome him, as I do all of you.

Today I plan to address major trends in nuclear reactor safety research in the U.S. and to place them in the context of other trends in the U.S. that affect the needs for and nature of reactor safety research in anticipation of the 1990's. My focus will naturally be on the reactor research programs of the U.S. Nuclear Regulatory Commission and their regulatory impacts, but I will try to identify how this work fits in with other water reactor safety research in the U.S. and the international community.

Let me highlight first in an overall fashion a few of the general considerations affecting research that you will hear again as I discuss each of the research areas.

I think our present research program in the U.S. is driven by the following factors:

First, the U.S. reactor safety research program today continues to show evidence of the research decisions made as a result of the accident at Three Mile Island. Thus, we have a strong emphasis in today's research on thermal-hydraulic transients, and, in particular, on small-break loss-of-coolant accidents (LOCAs). At present, much of this work is nearing completion, and thus we are moving into a transitional phase in which operating experience since TMI is becoming the major determinant. As this phase develops, we will find ourselves focusing more on applying our knowledge to plant specific sequences outside the design basis envelope addressed by the research program to date, and to simplifying our techniques to reduce analysis costs. This area of research is heavily influenced by the high degree of nonstandardization of reactors in the United States.

Second, as I am sure you all know, there have been no new power reactors ordered in the U.S. since 1978, and many of the reactors still in the pipeline are either nearing final licensing decisions in the next few years, or are indefinitely delayed and may never be completed. Coupled with this, DOE's advanced reactor research program has been refocused since the CRBR program was terminated. These realities have led to a deemphasis on research directed at the licensing of new reactors and a focus on research directed at regulating already operating reactors. Furthermore, the lack of a near-term need to address licensing issues for advanced reactor technologies has led to a reduction in future-oriented efforts aimed at new technologies. We presently maintain only a small program in this area. However, we cannot rule out that a new generation of reactors may be forthcoming in the 1990's with simplified systems, using new and untested materials and increased computer control of operations, and we continue to follow trends in the Department of Energy and in industry.

A related problem is the aging of the U.S. commercial power reactor inventory. As the first nation to have nuclear power, we also inevitably have some of the oldest operating reactors. With a continually increasing demand for electricity and limited new capacity under construction, we can readily predict that the utilities will be motivated to keep their existing plants operating beyond the original design life and to improve capacity factors. We can expect specific measures related to life extension of nuclear power plants such as: reduction of neutron flux in reactor vessels; improvements of surveillance and test methods for components or systems; upgrading the reliability of key components or systems; developing good reliability assurance programs; and making the modifications that come to light from resolution of safety issues. Therefore, the potential problems of aging plants have become a major focus of our research, and the implications of possible proposals for life extension are likely to make the problems even more important. Although we will face many of these problems sooner than other nations, [illegible] these trends will inevitably occur elsewhere in the world as well, so this is an area with good prospects for technology exchange and transfer. A further consequence of the aging of our plant inventory will inevitably be the need to decommission plants. While this issue has received considerable attention by the Commission, the focus of interest is mainly on economic, logistical and sociopolitical considerations rather than on scientific uncertainties, so I will not discuss it further.

Thirdly, the trend toward joint projects and greater cooperation in planning of programs, although largely driven by budget constraints, is, I believe, also improving the quality of the research. Nuclear safety issues are complex and require multidisciplinary, highly interactive efforts to resolve them. They need the cross fertilization that comes from having a number of participants. Further, we all have a parallel interest in the prompt resolution of nuclear safety research problems as they may arise. A problem for one is a problem for all. Solutions, when attained, benefit all.

A major result of this increasing trend toward cooperation in nuclear safety research is likely to be improved public confidence that nuclear safety decisions are based on good science. Cooperative programs can lead to a well-founded international consensus on the underlying science of safety issues. The existence of such a consensus and broad peer acceptance within the scientific community will in turn facilitate acceptance of research results by the public and by the political leadership. And it will be easier for regulators to apply these results to achieve more effective and less costly safety regulation.

Another factor which I previously alluded to is the general pressure to reduce Federal expenditures. This, coupled with the perception of some that all the reactor safety problems have been solved and that little or no further research is needed, has led to budget reductions in the last few years that have severely affected the NRC research program. In particular, it has required the termination of operation of older major and expensive facilities, such as the Power Burst Facility, that might still have been able to contribute valuable research results. Loss of this experimental capability may make it difficult to deal with issues which may be raised concerning the validity of the computer codes used to analyze severe accidents.

One final element of our research program that I should bring out is the difficulty created by the nonstandardization of reactor designs in the U.S. This is a difficulty which is not as severe in many other nations, which have moved more rapidly towards standardization than we have. We must, in most cases, examine separately the reactor designs from each major vendor, often for a number of different configurations. The consequences of nonstandardization have been particularly severe in PRA analysis, and where costly facilities are required, such as in the thermal-hydraulics area. I will try to highlight some of the impacts of this nonstandardization as I discuss specific elements of the research program.

Now, having highlighted these overall factors, I will discuss the current status of the five major reactor research areas in which we are most heavily engaged. These are: component aging and degradation, transient analysis, severe accident consequences, plant risk analysis, and seismic research. As I outline the major trends in the individual research areas, I think you will see how the overall factors have influenced the research directions in each case.

The first area of research relates to the mechanisms and significance of aging and degradation in service. As plants have matured, there have been a number of phenomena of corrosion, radiation embrittlement, fatigue and other effects which have raised questions about the continued safety and viability of the plants and, in particular, about the integrity of the primary coolant pressure boundary. Widespread and potentially serious problems have occurred. These have included cracked piping at BWRs, steam generator degradation at PWRs, defective valves and relays, and inadequate means for detecting and characterizing flaws.

We also face the fact that there may be degradation of other components or currently unrecognized degradation effects as plants continue to age. In response to this concern, the NRC has recently made several arrangements to take advantage of available materials and components to investigate whether there are degradation mechanisms not previously recognized. One such arrangement is in cooperation with the U.S. Department of Energy for components from the Shippingport reactor, and the other is in cooperation with the Federal Republic of Germany, for samples from the Gundremmingen reactor. Plans call for acquisition of samples during the next year, and our research will include examination of the effects of natural aging, and testing under design-basis event conditions to determine how aging affects the performance of safety functions. We are also giving a great deal of attention to environmental qualification programs, for both electrical and mechanical equipment, to develop testing techniques by which one can better predict performance throughout life.

Exacerbating the aging problem is the expectation that utilities, which are starting construction of little new capacity, will need to continue to operate existing plants well beyond their design life in order to continue to meet demands. The need for utilities to begin to make decisions is rapidly approaching. Based on the 40-year limitation of existing licenses, by the year 2001, operating plant licenses will begin to expire, and by 2015, 85 percent of existing licenses will have expired. The prospect of prolonged nuclear plant life has evoked some enthusiasm within the U.S. industry and among its suppliers and vendors. Our regulatory readiness to assess the merits of an application to extend an expired license is closely tied to the research we conduct over the next decade. In anticipation of applications for life extension from the utilities, NRC is beginning to explore the research implications of extended operation, including the cumulative damage to mechanical components from operational cycling fatigue and thermal and radiation-induced materials degradation processes. We will need to determine how to assure continuing safety through replacement of selected old components, addition of compensating systems or surveillance programs, and modifications in operations. The knowledge gained by research is indispensable here. How else will NRC be able to confirm regulatory assumptions and identify problems before they become major stumbling blocks? We must understand aging and its relationship to other factors such as availability and reliability. A full understanding is essential to making any regulatory decisions to permit extension of the life of a nuclear power plant, and to defining requirements for associated inspection and surveillance programs.

The second major area of research is that of system response to complex operational and thermal-hydraulic transients. In the first years of commercial nuclear power, work in this area was largely oriented toward large-break LOCA issues and reflected an underlying perception that the whole range of transients could be adequately characterized by analysis of an extreme limiting case. The TMI accident and more recent operating incidents on other plants have shown us that this is a questionable assumption, and in recent years the emphasis has shifted much more to small-break LOCAs and other transients and toward best estimate calculations rather than limiting case analyses.

As the range of transients considered has been broadened, it has become clear that, in addition to safety impacts, there are also potentially substantial economic benefits to the industry to be obtained from this research. That is, as research is oriented more toward best estimate codes and wider treatment of transients, capability to affect system reliability and on-line availability in a manner which is acceptable in a regulatory context is improved, and thus the research can offer a significant potential savings to the industry in addition to resolving safety questions. Partly reflecting this, the utility industry and reactor manufacturers have participated heavily in these programs in the last few years.

The approach we have been following in the thermal-hydraulic research program is: first, to conduct small-scale and separate effects tests to understand the basic phenomena; second, to demonstrate and validate codes with large-scale integral tests; and third, to conduct additional experimentation to assess the applicability of those codes to new problems as they may arise. Particular applications of the results are in the evaluation of plant transients, training of operators, simplification of operating procedures, and the development of a basis for plant analyzers which can be used widely in assessing operations and in developing appropriate responses to transients.

The need for continued assessment of code applicability to new configurations and transients is vitally important in the United States because of the lack of standardization in designs of U.S. reactors. For each unexpected transient that occurs in one plant, we must evaluate how it may apply to other reactors which differ to varying degrees in design and configuration. The importance of this type of analysis was graphically demonstrated by the June 9 loss of feedwater accident at the Davis-Besse plant. NRC follow-up and response were considerably enhanced and speeded up through rapid analysis of potential consequences and investigative actions. These analyses were performed on a prototype nuclear plant analyzer. A significant problem that we faced, however, in the course of these analyses, was ascertaining the applicability of available plant-specific data from another plant which was similar but not identical to Davis-Besse in evaluating the Davis-Besse transient and other related transients.

We are working toward integration of our thermal-hydraulic efforts. We are consolidating our code development work and evaluating various options for continued experimental work on new and unexpected transients as they develop at operating reactors. We have selected one type of code, the so-called TRAC series, from among the several being developed in parallel, and future work will concentrate on this series, which includes separate BWR and PWR versions of the code. We are willing, under appropriate conditions, to maintain other codes for use by research partners on a full cost-recovery basis. In the experimental area, because of the expected continuing need to assess code applicability for unanticipated transients, we are seriously considering options for long-term highly flexible integral experimental capability for testing of the type now done at the Semiscale facility and in FIST and MIST. Our options range from using existing facilities, either where presently located or consolidated in a single location, building a small-scale, or separate effects facility, or building a new larger-scale integral test facility. For any of these options, the ability to test transients in different reactor types and configurations is paramount. We anticipate making a decision in this area next year, and expect to consult broadly within the research community before making such a decision.

We can also anticipate that the limited construction of new electrical capacity in the U.S., and the generally low availability from our existing reactors -- as well as from the fossil plants -- will lead to efforts to improve the capacity factors of operating plants. Thermal-hydraulic research, as well as research in several other areas, could prove very valuable in assessing proposals from industry related to improving capacity factors.

The third area is that of severe accident research. Severe accident research is directed at ways of managing accidents. Accident management can be viewed as having three tiers -- the first is to prevent the accident altogether, the second is to arrest the accident, and the third is to mitigate its possible consequences. The primary objective of any severe accident research, of course, is always to prevent the accident. In that sense, the work on thermal-hydraulic transients which I have discussed can properly be seen as part of severe accident research. The rest of severe accident research can then be viewed as being directed toward either arresting or mitigating the accident. The understandings obtained from source-term studies are vital to both these areas.

The first of these two categories concerns those accidents which involve degradation of the core in an arrested sequence, that is, without significant core melt. Here the problems are those of fission product release, plate-out, and other things that primarily affect the safety of continued operation. The phenomenology of such accidents is near resolution, at least in regard to what is pertinent to such regulatory considerations as siting and emergency planning. There is, of course, a longer-term problem in such accidents, relating to the radioactivity distribution within the plant and its systems in such arrested sequences. That problem requires much more work, but it is primarily an industry problem with only limited concern to regulators because it does not impact significantly on the health or safety of the public.

The second category under severe accident research is that of severe core degradation, that is, accidents involving core melting. Here the major issues are: coolability of degraded cores; loads on containment that occur as a result of core meltdown, for example, from gas production or effects of core concrete interaction; and containment failure modes. The last of these is particularly critical. Containments are full of penetrations and irregularities, and the failure mode can be very complex. Because early containment failure is a major element in assessing risk, we believe this is an important area.

The fourth major area is research related to reactor operations and probabilistic risk assessment. Here the main issues relate to the application of the insights obtained from the two areas which I have just discussed to plant operations -- to assessments of operational capabilities, instrumentation, monitoring, control room design, operating procedures and operator training, technical specifications, and generally to incorporation of these insights into the human factors areas.

There clearly is a need for significant improvement in PRAs if they are to be fully useful. These improvements can be broken down into two categories. The first is an improvement in the data base. That means better data on component reliability, better data on maintenance experience, and better feedback from operating plants. It also means incorporation of the insights and understanding that have been developed from the thermal-hydraulic program, and from source term reassessment. A second important aspect of PRAs which needs improvement is the methodology itself, by incorporation of human factors and common cause failure modes such as might arise from earthquakes, internal and external flooding, or sabotage. These factors have been found to be major contributors to risk in some cases, so their incorporation into PRAs is important. Now that the first phase of PRA development, which focused on the modeling of electrical and mechanical event sequences, is relatively well advanced, the next logical step is the development of improved models in the areas of human factors, earthquakes, flooding and sabotage. We are therefore directing research attention toward several of these areas, and recent and planned efforts should lead to improvements in PRA where these factors are involved.

Initially, PRAs are being applied in the United States to specific licensing decisions, to decisions related to a particular design, and to prioritizing unresolved safety issues and research needs. Recent applications of PRA in NRC have included their use in the resolution of such unresolved safety issues as anticipated transient without scram (ATWS), station blackout, decay heat removal, and pressurized thermal shock. We can also anticipate that PRAs will be useful in helping assess possible implications of industry proposals to upgrade plant capacity factors. Ultimately, with significant improvement in the methodology and data base, some application in terms of safety goals or establishment of quantified levels of acceptable risks will follow.

The final major area of reactor research is seismic safety margins research. Here, of course, we share the greatest interest with other countries, such as Japan, which lie in zones of relatively high seismicity. With the continuing improvement in understanding of geology and seismology in recent years we have come to realize that a number of the older plants may have been designed for seismic hazard levels that are not high enough, particularly in the Eastern U.S. Furthermore, PRAs which have been done have shown that seismic risk is a significant severe accident contributor. Thus, although we believe that there is a considerable seismic safety margin in present designs, we see a need for well-validated improved seismic analysis methods which could be used to reassess these currently operating plants, to assure that margins do exist and ideally to quantify those margins to some degree.

In addition, experimental data that can be used to validate the nonlinear models for seismic response of structures which have been developed is also needed. This is the sort of research that requires extensive and expensive shake testing, and is well suited to collaborative arrangements. Internationally, NRC has an agreement with KfK of the Federal Republic of Germany to participate in the HDR program in which they are shaking that facility to determine piping response; we are also negotiating with the Nuclear Power Engineering Test Center in Japan to participate in testing in the Tadotsu facility. We are working very closely with EPRI in this whole matter of seismic research.

Also, a simplified design approach that could be compared against, or benchmarked against, these validated nonlinear methods is needed. Such an approach could identify simplifications that could be permitted in present plants, and hopefully for future plants will lead to much simpler and more straightforward designs.

A major recent development has been the proposed modification of General Design Criterion 4 of our regulations to permit removal of pipe restraints that are unnecessary, costly, and impediments to safety. These pipe restraints were originally required to protect against the large double-ended guillotine pipe break. New evidence suggests that the probability of such pipe breaks is very small and that the restraints, if improperly installed, can produce undesirable stresses. Further, the restraints in general have made maintenance and other procedures more difficult, increasing worker exposures, costs, and installation errors. Removing the requirement to use such restraints, in addition to improving seismic safety and occupational safety, will also result in considerable cost savings for both new and operating reactors. This is an example of an application of our work on piping system behavior in which we take great pride.

Of course, the U.S. NRC is not alone in conducting reactor safety research in these areas which I have discussed; and, as I mentioned earlier, a number of factors are driving us more and more toward collaborative research efforts with other organizations having similar interests. These factors include, of course, the costs of research and our budget constraints. But I should emphasize that they also include a desire to reap the benefits of cross-fertilization in research, to contribute to reactor safety worldwide in areas in which we have a strong research capability and to benefit from the efforts of others, both within the U.S. and internationally, in areas in which they have strong capabilities. Therefore, we continue to place a strong emphasis on joint research programs with both domestic and foreign partners.

Clearly, we and our domestic partners share obvious common goals, although we may approach our research from somewhat different perspectives. Time does not permit a lengthy discussion of our domestic cooperative efforts. Suffice it to say that we have worked closely with both industry groups such as EPRI, IDCOR, and owners groups, and with major U.S. reactor manufacturers. In the international area, the common interest can be demonstrated graphically by the fact that, although there are some differences in emphasis, there is substantial general agreement among the members of the international nuclear safety community on the need for research in the areas I have discussed.

In conclusion, at present we believe that need for maintaining nuclear reactor safety research capability continues to be strong, and the U.S. program is focused on meeting the coming requirements of an aging complement of reactors. Present budget constraints and increasing research complexity have made collaborative research efforts increasingly attractive, and we anticipate a continued trend toward joint efforts with other organizations to share facilities, personnel, and perspectives.

# NUCLEAR SAFETY RESEARCH: THE SPANISH PERSPECTIVE

Eduardo González Gómez, Commissioner
Spanish Nuclear Safety Commission
Madrid, Spain

Presented at
Thirteenth Water Reactor Safety Research Information Meeting
October 22, 1985

Mr. Chairman, ladies and gentlemen, good morning.

I first of all want to thank the Nuclear Regulatory Commission on behalf of the Spanish Nuclear Safety Commission for giving me the opportunity to speak to such a distinguished audience. This honor cannot be based upon the past Spanish contribution to the Nuclear Reactor Safety Technology, but I hope that it will contribute to the future activities in this field in my country, which, as we shall see, is in the process of reaching the level where it should have been, taking into account the history and the reality of the Spanish Nuclear Program. I would like also to apologize for my English and I hope you will forgive me if it is not correct. I will try my best.

This morning, it is my intention to express a few considerations about the Nuclear Safety Research Program justification and needs, and afterwards to have a glance at the actual program that we are developing in Spain. Very often, in my country a question is asked of me, "How is it that with so many nuclear reactors in operation in the world, which you say are proven to be safe enough, and with the stopping of the construction of new plants because of economic reasons, is there a need for further research in reactor safety"?

This rhetorical question does not have a very easy answer, but if you examine the experience that we have had up to now, there is no doubt that a better knowledge of the processes, materials and components that constitute a nuclear power plant will contribute to a more reliable and safe installation.

The Three Mile Island accident showed this need. At that time it was seen that there was not enough comprehension of the sequence of events that led to that emergency situation nor of the effects that the metal-water reaction could have, not to say that the ideas about the situation of the damaged fuel had been very far away from the real situation of the core, which by the way could show some of the conservatisms that exist in the actual evaluation of accidents.

Other incidents with materials have arisen like the stress corrosion cracking of the stainless steel recirculation piping in the BWR or the vibration problems that arose with the steam generator tubes of the D-3 Westinghouse model, not to mention the different sicknesses that these same tubes have had.

There have been also operational incidents with the malfunctioning of valves, equipment failures or human errors that have led to situations that, if not managed properly, could have gone to the limit. Besides all these reasons related to the problems that the operation of the installations will show, we cannot forget, from the regulatory and licensing side, that we will have to continue to make decisions about the different situations that will arise, assessing them so that they can be accepted or so that the need for further analysis or additional features is established.

We have to realize that the level of the consequences of decisions by the regulatory authorities is of such importance that there is a real need for them to know better what is happening if they have to be responsible for their assessments. Another aspect has to be considered, and it is that of the conservatisms that exist in the regulation. The production of nuclear energy has to be accomplished with all safety assurances, but this does not mean that unrealistic conservatisms are necessary. They could mean a burden to the plant in the economic aspects, but they also could cover the real problems that may exist, not allowing us to estimate adequately the processes that take place. Finally, an aspect that cannot be forgotten is that new developments may come out and the authorities have to be prepared to assess them. Care should be taken not to lose brilliant experts that would be very difficult to replace if they started to work in other activities. In that respect we have to think that the operation of existing plants will have its own development and that the evolution of the techniques that will be utilized for the extension of the plant life would have to be followed and assessed by the licensing authorities.

If we agree on the need to continue the existing efforts in the nuclear safety research activities, an aspect that we have to consider is how to organize and coordinate the efforts that are being made. For the larger countries, or the ones that have important developments in nuclear energy, they could have the temptation to try to attack the problems by themselves. In fact, even though the developments that were made in the United States, back in the sixties, have been utilized by most of the western countries so that very often they have abandoned their own development (like France did with the graphite-gas reactor or the British are accomplishing with their Sizewell Inquiry to get to a new reactor model), countries like the United States, France, Japan or the Federal Republic of Germany could have enough experience and capacities to approach the research activities by themselves. Even in that situation the need for interchange of information is clear.

Besides that, and taking into account the standardization that exists world-wide in the basic processes and also the economic situation, a tendency has been growing to develop the new programs through international multilateral agreements.

This is the only opportunity that the medium and small sized countries may have to participate in the research programs that need important investments. At the beginning of the utilization of nuclear power in medium countries which did not have a science and technology infra-structure, what they did, and Spain is a very good example, was to accept the concepts and patterns that had been developed in the country of origin of the project. This decision was made very often, abandoning developments that had been undertaken and it meant that the country tried to use the foreign technology losing some of its independence as national criteria were very often put aside.

The system that was established had benefits in the development of the utilization of the technology, but it had some drawbacks in the sense that the basic technological knowledge was not accessible to the buying country that lost the capacity to make decisions on its own or to perform its own development.

In Spain, for example, minimum percentage of Spanish participation in the investments was established so that they have gone from 40 percent in the plants of the first generation to 80 percent in the plants that are now under construction but these have been direct investments in the installation.

It was felt that we were financing the research that was done in the countries where the systems were developed so that a double investment should not be made. A paradox developed by which, when we started an important nuclear power plant program, we stopped our own development in the applied research in the area. The consequences were, as I have said, that a lack of proper capacity was visible and among other things it had a very negative impact on the public opinion since the public did not have enough confidence in the technologists of their own country and in the installations that they were running.

The situation today is changing and I hope that medium-sized countries, and especially Spain, will get more involved in the international effort so that we can know better what our specific needs may be and we can then decide in which fields we have to develop activities in our own country.

At the international organizations level the interchange of information is taking place, and today they are the forum where many of the mainlines are discussed. The Committee for Safety of Nuclear Installations (CSNI) of the Nuclear Energy Agency in Paris is in that sense a point of contact for all interested parties in this matter. During 1985, through an ad hoc group on "Priorities in LWR Safety Research," the areas where safety research would have to be performed in the future were analyzed. This ad hoc group was given a mandate to discuss the objectives and rational that should govern LWR Safety Research as well as to review its current status. Additionally, they should identify the areas which require substantial efforts in the future. They should also study the different roles of industry and government in funding the necessary research and finally determine the possibilities of increased international cooperation.

The final conclusions of the work performed have not been yet approved by the CSNI but I understand that five main areas have been identified in which continuing safety research could be necessary.

These areas are the following:

- Equipment and structures integrity that would include materials, non-destructive examination, seismic hazards and aging problems;
- Plant transients with computer simulation codes and performance of tests;
- Plant operations where operating procedures and training as well as technical specifications and human factors coupled with the plant analyzer development and instrumentation should be dealt with;
- Risk analysis and reliability with the use of probabilistic methods in safety analysis, common cause failures and the development of a reliability data base, and
- The severe accident issue with source term generation and transfer, the containment performance and accident prevention and management as well as the environmental consequences.

I am convinced that these areas will have to be dealt with in the future, but I would not like to forget two issues that have to be studied and improved for a more reliable and safety-approved nuclear power program. These are the waste issue and the radiological protection of workers.

The funding issue is also of a great importance for it would be very important to show the independent capacity of the administration.

In a time of economic crisis, decision makers could be tempted to eliminate the independent role of authorities thinking that they only cause delays and problems for the industry. If such a step were taken, the harm that will be done to the credibility of the system will be very strong. The role of independent organizations as mediators between the public and the industry is of vital importance for the industry itself.

Finally, an aspect that has to be clearly enhanced is the international cooperation which, as I have said before, will be the only opportunity that medium and small sized countries will have to participate in the developments. After these general remarks, I will take some of your time to explain to you how this whole picture is seen from Spain.

Spain is a small country that has an important nuclear program. In 1984, 20% of the total electricity production which runs in the order of 100,000 gigawatts x hour was generated with nuclear energy. In 1985, we expect this share to grow to about 25% and by 1986 it will go to 30% where we think it will stay for some time. Our installations started to operate in 1968 when Jose Cabrera, a 180 MWe pressurized water reactor from Westinghouse was hooked to the grid. At that time two more plants were constructed, one of French origin, Vandellos I, a graphite-gas reactor of 480 MWe and the other, Santa Maria de Garoña, a 460 MWe boiling water reactor from General Electric. All these plants were constructed on a turn-key basis with a 40% Spanish participation in the investment and they have always been run with Spanish technicians.

At the beginning of the seventies, and after many discussions related to the optimum plant size, seven additional plants were ordered, in the range of 1000 MWe. Six of them were from Westinghouse and one from General Electric.

The potential electrical output that was under construction in the mid-seventies was 6460 MWe and these plants were constructed mostly with Spanish management and technicians so that the national participation in the investment rose to about 70%. All these plants are now in operation except the two groups of the Lemoniz Nuclear Power Plant whose construction was stopped so that the actual operating capacity is about 5600 MWe.

With that installed capacity and with two more groups that will go on line before 1990, the activity in the research area has been up to now very limited.

The operation of the existing plants has not caused any major problems but four issues have been of interest.

The first one has been the backfitting of both the pressurized and boiling water reactor that went into operation at the end of the sixties, about fifteen years ago. For both plants an evaluation was made and they have undergone modifications and improvements that have meant an investment close to 100 million U.S. dollars for each one.

The second issue was the modification of the Westinghouse Steam Generator Model D-3 which had flow-induced vibrations in the preheater area discovered in the Ringhals Plant in Sweden. This problem affected all the Westinghouse Spanish plants of the second generation and has caused many delays in the starting of the plants. One of the plants had to work at 50% power for more than a year.

The third issue has been the stress corrosion cracking of the recirculation pipe at Santa Maria de Garona where the problem was discovered in 1983 and which is now in the process of substituting all of the 304 material with 316 nuclear grade material.

The last issue that I wanted to comment on is that of the development of emergency planning. Since 1983 a very large effort has been made in this field. At the beginning it caused many public opinion problems but we have reached a point where it has been shown to the authorities and to the public the importance of the effort that was made in this field.

Besides these technical issues, a major decision was made at the beginning of the eighties to create a licensing and inspection organization, independent of the government, which reports directly to the Spanish Congress and is in charge of all nuclear safety and radiation protection matters. This organization, the Consejo de Seguridad Nuclear (CSN), to which I belong, was created following the lines of the Nuclear Regulatory Commission, and it has in its charter the whole responsibility for the safety conditions that have to be imposed on the plants for licensing and operation. The CSN is also entrusted with the responsibility to establish research programs in nuclear safety and radiation protection. For that the CSN created a National Committee for Research in Nuclear Safety and Radiation Protection, where all the institutions of the country that dealt with these matters participated.

The government through several ministries like Industry and Energy, Education and Health, participated as well as the more important research centers like the Junta de Energia Nuclear and the Seismological and Geographical Institutes. The universities, and the industry, through the electrical utilities and the fuel and main component fabricators, are members of this committee which is chaired by the president of the CSN.

This Committee created five working groups headed by CSN members so that the different areas could be covered.

- Group 1 was in charge of siting;
- Group 2 was in charge of operation;
- Group 3 was in charge of the fuel side;
- Group 4 was in charge of radiation protection and ionizing radiation effects; and Group 5 was in charge of materials, components and structures.

The working groups were mandated to analyze the existing national activities in their field of interest, as well as the international ones, and to define areas where work should be done establishing priorities for it.

The conjunction of the international programs with the national ones was a condition to participate in any outside plan. The final results of the work of this National Committee have not been released but we can cover the main areas of work.

For the first working group in charge of siting, we have to realize that in Spain we are in the process of deciding the nuclear waste repositories that will be utilized both for low-and high-level radioactive wastes. In that respect the main program is the development of a Neotectonic, Seismotectonic, and Seismic Risk Map that will collect information to be utilized in the selection of sites.

The second working group, in charge of the operation of nuclear power plants, has been studying the problems of the management of the plants dealing with maintenance, inspection, training of personnel including plant analyzer, the human factor issue and the operational transients. In that respect Spain is participating in the LOFT Program and the CSN has approved the signature of an agreement with the NRC that includes the utilization of thermohydraulic and fission product behavior codes, and participation in the Severe Accident Program. Besides that, we are discussing with the French authorities the participation in the Phebus program.

The working group number three was in charge of the fuel cycle and in that area the stress has been put on the waste conditioning and management. We participate in the International project of the OECD and we are developing a national storage and transport cask for fuel elements called Centauro.

Working group number four dealt with radiation protection. The radiological impact of the nuclear installations and radioisotopes-related activities, the epidemiological studies of the population subject to radiation exposures, the effects of ionizing radiation on living tissues, the synergistic effects of temperature and radiation are some of the projects that are underway.

As for working group number five, responsible for activities in materials, components and structures, the main fields of interest have been the materials themselves; for example, the intergranular stress corrosion cracking, crack arrest and aging issues, the nondestructive examination techniques with emphasis on U.T. and eddy current and also in the acoustic emission, the analysis of concrete structures, the probabilistic risk analysis and the equipment qualification.

In that respect we participate in the PISC II and PISC III programs of the NEA, in the International Program of the Atomic Energy Agency on Irradiated Vessel Materials and in the TRANS RAMP Project at Studsvik that is studying the pellet-cladding interaction in nuclear fuel.

Additionally, we are evaluating the convenience of our participation in the IPIRG effort.

In the probabilistic risk analysis area we are preparing an evaluation of the risk of the Spanish nuclear power plants and this is one of the fields of collaboration in our agreement with the NRC. This information may have been very detailed but I wanted you to know about the effort in the research area that has been undertaken and also in the coordination of different national institutions. I would like to point out especially the bilateral agreements with the NRC and the IPSN from France and the international participation in projects like LOFT which could be an example for future activities.

Let me finish my talk with some conclusions that I feel are important for the future of nuclear safety research. The need for a continuing effort is clear if society wants to operate a reliable and safe nuclear system. Future development makes it necessary to keep the capacity and to apply the lessons learned during the actual operation. The independence of the activities in the research field of the authorities is necessary for the work itself and for the credibility of the organization. The research activities have to be as close to reality as possible so that the problems that arise are realistic and effort is dedicated in the right direction. This is an international world and efforts have to be coordinated and information interchanged.

Thank you very much for your attention.

# OPERATIONAL PHASE OF

# INSPECTION PRIORITIZATION

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Inspectors must make many decisions on the allocation of their efforts. To date, these decisions have been made based upon their own judgment and guidance from inspection procedures. Our goal is to provide PRA information as an additional aid to inspectors. A structured approach for relating PRA information to specific inspection decisions has been developed. The use of PRA information as an aid in optimal decision making (1) in response to the current plant status and (2) in the scheduling of effort over an extended period of time is considered.

## INTRODUCTION

The Risk Assessment Application to NRC Inspection Program being performed by NRC contractor personnel at JBF Associates, Inc. is developing methods for applying the results of probabilistic risk assessments (PRAs) to manpower allocation decisions made by NRC inspectors. Results of this work will help inspectors to optimize their influence on plant risk.

Two key observations made early in the first phase of this program have had a major influence on the program's direction. First, PRAs are limited to quantifying the bottom line risk for a plant and showing how important various component and system failures are to this risk. While inspection personnel do inspect individual components and systems, inspectors are validly more concerned with assuring that nuclear power plant owners have adequate reliability assurance programs in place. Equipment reliability performance is a useful barometer for evaluating a licensee's programs; however, when equipment performance suffers, inspection personnel are more effective if they focus on the root causes of failures and associated corrective actions rather than responding to individual failure events.

With this observation in mind, Phase I of the program developed a four-step procedure to relate PRA results to inspection decisions. These steps are:

1. Relate system and component failure probabilities to plant risk

2. Relate root causes of failure to system and component failures

3. Relate reliability assurance programs to root causes of failure

4. Relate inspection actions to reliability assurance programs

The first step is accomplished by using the results of a PRA. The second step, relating root causes of failure to system and component failures, is the key step in this procedure. If the various root causes of failures can be ranked according to their importance to plant risk, then the door is opened for inspection personnel to carry out the last two steps. Thus, the gap between PRA results and the needs of inspection personnel can be bridged by identifying the relationships between root causes of failure and system and component failures. The NRC has programs in progress to evaluate root causes of failure.

The second observation that influenced the direction of the Risk Assessment Application to NRC Inspection Program is that PRA reports are written in the language used by PRA practitioners--a language that is not readily understood by others. Phase II focuses on developing a program for use on a microcomputer. This aim is to present PRA-based information that can be easily interpreted by inspectors for use in making decisions. The Plant Risk Status Information Management System (PRISIM) is a decision-oriented, user-friendly, menu-driven program that contains data base management and interactive routines to aid inspectors in allocating their efforts toward those areas of greatest impact on safety. The facility chosen for demonstrating PRISIM is Arkansas Nuclear One-Unit 1 (ANO-1).

A computer program was chosen to catalog and present the PRA information since the total amount of information is large but the amount needed for any particular decision is relatively small. PRISIM allows the user to quickly and logically access the desired PRA information without being overwhelmed by enormous quantities of data. PRISIM's data base consists largely of screen images that present PRA information in both textual and graphic formats. Each screen image also acts as a menu, giving the user options to see more-detailed information in the area of his interest. The user does not need to have a background in computer operation or PRA to use the program or to understand and employ the information it presents.

PRISIM is designed to aid inspectors in making two types of decisions. The first type is deciding whether to respond to a particular event or plant condition, and if a response is warranted, deciding where to focus effort. The second type is deciding how to schedule routine inspections so that emphasis is placed on systems and components where the risk impact of inspections is greatest.

The interactive portion of PRISIM is useful in deciding whether to respond to a plant condition, and the interactive portion in conjunction with the data base management portion is useful in deciding where to focus efforts if a response is required. Use of PRISIM for this type of decision making is demonstrated in the first example that follows.

The data base management portion of PRISIM provides information that can aid inspectors in scheduling routine inspections. Use of PRISIM for these types of decisions is demonstrated in the second example that follows.

Both of the following examples make extensive use of figures that are prints of PRISIM screen images. An arrow drawn on a screen image shows the cursor position that would take the user of PRISIM to the screen shown in the next figure.

## Example 1 - Response Decision

Figures 1-13 demonstrate how PRISIM can be used as an aid in making decisions associated with responding to the current plant status. For this example, assume the inspector has visited the control room and found that a valve in Train A of the Emergency Feedwater System and a valve in Train A of the Battery and Switchgear Emergency Cooling System are out of service. He consults PRISIM to determine the significance of this plant condition.

The inspector is first presented with the option of obtaining safety-related information through a direct access path or a module-related path (Figure 1). Having selected the direct access path, the user is presented with a list of information categories addressed in PRISIM (Figure 2). For this case, he selects "Risk Implications of the Current Plant Status." He is then presented with screens that allow him to specify the out-of-service components (Figures 3-8). Note that once the inspector has specified the component that is out of service in Figure 6, he then selects "SYSTEM MENU" so he can specify the second out of service component.

Once the user has specified the out-of-service components in Figure 9, he selects the "END OF INPUT" option and is presented with a screen (Figure 9) that (1) lists the components specified as being out of service, (2) identifies the factor of increase in core melt frequency, and (3) provides options for additional information. For this case, the core melt frequency is increased by a factor of 70.0.

To obtain information that will aid him in deciding how to focus his efforts on the components not known to be out of service, the user selects the "Component Priority" option at the bottom of the screen. He is then presented with a screen that provides a priority listing of components not known to be out of service (Figure 10). Priority components are those components currently most vital to keeping the plant safe. For this example, the inspector knows that maintenance is to be performed on the EFW Train A-to-Train B crossover line, which is one of the listed components. To assess the impact of this situation, the user again enters the program (several screens not shown), specifies the additional out of service condition (Figures 11 and 12), and is presented with an updated increase in core melt frequency (Figure 13). The user can now employ this new information as an aid in deciding whether to respond to the plant status.

## Example 2 - Scheduling Decision

Figures 14-21 demonstrate how PRISIM can be used as an aid in making a scheduling decision required by the inspection procedures. As the inspector begins, he is again presented with the option of obtaining safety-related information through a direct access path or a module-related path (Figure 14). Having selected the module-related path, he is presented with a list of inspection procedures addressed in PRISIM (Figure 15). For this case, we have the user selecting Procedure 71707 and he is presented with the decisions associated with that procedure for which PRA information is available (Figure 16).

To conduct weekly inspection activities, Procedure 71707 requires that the inspector confirm the operability of a selected safety-related subsystem. To maximize the impact of performing this procedure, the inspector will want to inspect all safety-related subsystems over a period of time. However, he will want to inspect more often those that are most significant to risk than those that are less significant. Under "Weekly Inspection" we have, therefore, designated the first decision as "Which safety-related subsystems should be emphasized?"

Having selected this decision, the user is presented with the appropriate PRA information (Figures 17-21). By using the information provided, the inspector can now make a decision on the proper allocation of his time.

## PRISIM Implementation

Demonstrations of the PRISIM program being developed for the Arkansas Nuclear One-Unit 1 (ANO-1) facility have been given to the Executive Director of Operations and his staff, Inspection and Enforcement headquarters personnel, and Region IV inspectors and management. Response has been very positive on both the information provided and the use of a personal computer to present the information. Also, suggestions for improvements were made in the demonstrations; we have incorporated these into PRISIM. PRISIM will be installed at ANO-1 and Region IV early next year where it will undergo field testing and be revised based on comments from the inspectors who use it.

PATHS FOR OBTAINING SAFETY-RELATED INFORMATION

1. Direct access path
2. Module-related path

Figure 1

CATEGORIES OF SAFETY-RELATED INFORMATION

1. Risk implications of the current plant status
2. Dominant accident sequences
3. Safety-related systems
4. Safety-related subsystems
5. Safety-related components
6. Support system interfaces
7. Component failure data
8. Fire zones

Figure 2

OPTIONS FOR SPECIFYING OUT-OF-SERVICE COMPONENTS

1. Schematics
2. Component List

Figure 3

SAFETY-RELATED SYSTEMS FOR WHICH COMPONENT LISTS ARE AVAILABLE

1. Battery and Switchgear Emergency Cooling System
2. DC Power System
3. Emergency AC Power System
4. Emergency Feedwater Initiation and Control System
5. Emergency Feedwater System
6. Engineered Safeguards Actuation System
7. High Pressure Injection System
8. High Pressure Recirculation System

(CONTINUED)

Figure 4

EMERGENCY FEEDWATER SYSTEM
COMPONENTS THAT MAY BE TAKEN OUT OF SERVICE

1. Condensate Storage Tank Block Valve CS19 or

   Condensate Storage Tank Outlet Check Valve CS98 or

   Condensate Storage Tank Outlet Check Valve CS99

2. Train B

3. Steam Generator A Emergency Feedwater Supply Check Valve FW13A

4. Train A-to-Train B Crossover Line

(CONTINUED)

SYSTEM MENU

END OF INPUT

Figure 5

EMERGENCY FEEDWATER SYSTEM
COMPONENTS THAT MAY BE TAKEN OUT OF SERVICE (CONTINUED)

5. Pump P7A Outlet Check Valve FW10B

6. Train A

7. Steam Generator B Emergency Feedwater Supply Check Valve FW13B

8. Emergency Feedwater Pump P7A Turbine Steam Supply Valve CVY-1

9. Emergency Feedwater Pump P7A Turbine Steam Supply Valve CVY-2

SYSTEM MENU

END OF INPUT

Figure 6

SAFETY-RELATED SYSTEMS FOR WHICH COMPONENT LISTS ARE AVAILABLE

1. Battery and Switchgear Emergency Cooling System
2. DC Power System
3. Emergency AC Power System
4. Emergency Feedwater Initiation and Control System
5. Emergency Feedwater System
6. Engineered Safeguards Actuation System
7. High Pressure Injection System
8. High Pressure Recirculation System

(CONTINUED)

Figure 7

BATTERY AND SWITCHGEAR EMERGENCY COOLING
SYSTEM COMPONENTS THAT MAY BE TAKEN OUT OF SERVICE

1. Chilled Water Train B
2. South Switchgear Room Cooling Unit
3. Chilled Water Train A
4. North Switchgear Room Cooling Unit

SYSTEM MENU

END OF INPUT

Figure 8

CORE MELT FREQUENCY INCREASE AND
OPTIONS FOR ADDITIONAL EXISTING PLANT STATUS INFORMATION

LIST OF COMPONENTS KNOWN TO BE OUT OF SERVICE

EMERGENCY FEEDWATER SYSTEM-TRAIN A FAILS
BATTERY AND SWITCHGEAR ROOM COOLING SYSTEM-CW TRAIN A FAILS

FACTOR OF INCREASE IN CMF IS 70.0

OPTIONS FOR ADDITIONAL INFORMATION

1. Repair Ranking
2. Component Priority
3. Failure Mode Priority
4. Return to Master Menu

Figure 9

PRIORITY LISTING OF COMPONENTS NOT KNOWN TO BE OUT OF SERVICE

1. BATTERY AND SWITCHGEAR ROOM COOLING SYS-CW TRAIN B FAILS
2. BLOCKAGE OF EFW TRAIN A-TO-TRAIN B CROSSOVER LINE
3. BOTH SAFETY/RELIEF VALVES FAIL TO RECLOSE
4. AUXILIARY COOLING WATER SYSTEM ISOLATION VALVE CV3643 FAILS
5. INTERMEDIATE CWS ISOLATION VALVE CV3020 FAILS
6. EFW INITIATION AND CONTROL-VECTOR SIGNAL PATH 1D-32D FAILS
7. EFW INITIATION AND CONTROL-VECTOR SIGNAL PATHS 2D-22D AND 3D-22D FAIL
8. BOTH SAFETY/RELIEF VALVES OPEN AND FAIL TO RECLOSE
9. HIGH PRESSURE INJECTION SYSTEM PUMP P36C FAILS
10. EFW INITIATION SIGNAL PATHS AC01-AC04 AND BD01-BD04 FAIL

ESC to return to the Selection Menu.

Figure 10

SAFETY-RELATED SYSTEMS FOR WHICH COMPONENT LISTS ARE AVAILABLE

1. Battery and Switchgear Emergency Cooling System

2. DC Power System

3. Emergency AC Power System

4. Emergency Feedwater Initiation and Control System

5. Emergency Feedwater System

6. Engineered Safeguards Actuation System

7. High Pressure Injection System

8. High Pressure Recirculation System

(CONTINUED)

Figure 11

EMERGENCY FEEDWATER SYSTEM
COMPONENTS THAT MAY BE TAKEN OUT OF SERVICE

1. Condensate Storage Tank Block Valve CS19 or

   Condensate Storage Tank Outlet Check Valve CS98 or

   Condensate Storage Tank Outlet Check Valve CS99

2. Train B

3. Steam Generator A Emergency Feedwater Supply Check Valve FW13A

4. Train A-to-Train B Crossover Line

(CONTINUED)

SYSTEM MENU END OF INPUT

Figure 12

CORE MELT FREQUENCY INCREASE AND
OPTIONS FOR ADDITIONAL EXISTING PLANT STATUS INFORMATION

LIST OF COMPONENTS KNOWN TO BE OUT OF SERVICE

EMERGENCY FEEDWATER SYSTEM-TRAIN A FAILS
BATTERY AND SWITCHGEAR ROOM COOLING SYSTEM-CW TRAIN A FAILS
BLOCKAGE OF EFW TRAIN A-TO-TRAIN B CROSSOVER LINE

FACTOR OF INCREASE IN CMF IS 174.3

OPTIONS FOR ADDITIONAL INFORMATION

1. Repair Ranking
2. Component Priority
3. Failure Mode Priority
4. Return to Master Menu

Figure 13

PATHS FOR OBTAINING SAFETY-RELATED INFORMATION

1. Direct access path
2. Module-related path

Figure 14

INSPECTION PROCEDURES ADDRESSED IN THIS PROGRAM

| PROCEDURE NUMBER | INSPECTION PROCEDURE TITLE |
|---|---|
| 71707 | Operational Safety Verification |
| 61726 | Monthly Surveillance Observation |
| 62703 | Monthly Maintenance Observation |
| 71710 | ESF System Walkdown |
| 92700 | Onsite Followup of Events at Operating Reactors |
| 92702 | Onsite Followup of Written Reports of Nonroutine Events |
| 92703 | IE Bulletin/Immediate Action Letter Followup |
| 71711 | Review of Plant Operations |
| 72701 | Review of Plant Operations |
| 92717 | Followup IE Circular, Information Type Bulletin |
| 61701 | Surveillance - Refueling |
| 62701 | Maintenance - Refueling |
| 92706 | Independent Inspection Effort |
| 92704 | Followup - Headquarters Requests |
| 92705 | Followup - Regional Requests |

Figure 15

DECISIONS ASSOCIATED WITH INSPECTION PROCEDURE 71707--
OPERATIONAL SAFETY VERIFICATION--FOR WHICH PRA INFORMATION IS AVAILABLE

DAILY INSPECTION

1. What emphasis should be given to the existing plant status? (Interactive Routine)

WEEKLY INSPECTION

1. Which safety-related subsystems should be emphasized?

BIWEEKLY INSPECTION

1. What safety-related tag-outs should be emphasized?
2. When a plant tour is performed, which fire zones should be emphasized?
3. Based upon the information collected during the plant tour, what emphasis should be given to the existing plant status? (Interactive Routine)

Figure 16

SUBSYSTEMS GROUPED BY RISK SIGNIFICANCE IMPORTANCE

HIGH RISK SIGNIFICANCE

Battery and Switchgear Emergency Cooling System-Chill Water Train A

Battery and Switchgear Emergency Cooling System-Chill Water Train B

Emergency Even DC Power

Emergency Feedwater Initiation and Control System-Initiation Channel A

Emergency Feedwater Initiation and Control System-Vector C

Emergency Feedwater System-Train B (Turbine Driven)

Emergency Odd AC Power

Emergency Odd DC Power

Emergency Onsite AC Power

(CONTINUED)

Figure 17

SUBSYSTEMS GROUPED BY RISK SIGNIFICANCE IMPORTANCE (CONTINUED)

HIGH RISK SIGNIFICANCE

Emergency Onsite DC Power

High Pressure Injection System-Train C

Service Water System-Loop 1

Service Water System-Loop 2

Safety/Relief Valves Fail to Close

MODERATE RISK SIGNIFICANCE

Emergency Even AC Power

Emergency Feedwater Initiation and Control System-Initiation Channel B

(CONTINUED)

Figure 18

SUBSYSTEMS GROUPED BY RISK SIGNIFICANCE IMPORTANCE (CONTINUED)

MODERATE RISK SIGNIFICANCE

- Emergency Feedwater System-Train A (Motor Driven)
- Engineered Safeguards Actuation System-Analog Channel 1
- Engineered Safeguards Actuation System-Analog Channel 2
- Engineered Safeguards Actuation System-Analog Channel 3
- High Pressure Injection System-Trains A and B
- Low Pressure Recirculation System-Train A
- Low Pressure Recirculation System-Train B
- Safety/Relief Valves Fail to Open

(CONTINUED)

Figure 19

SUBSYSTEMS GROUPED BY RISK SIGNIFICANCE IMPORTANCE (CONTINUED)

LOW RISK SIGNIFICANCE

- Battery and Switchgear Emergency Cooling System-Refrigerated Unit A
- Battery and Switchgear Emergency Cooling System-Refrigerated Unit B
- Core Flood System-Train A
- Core Flood System-Train B
- Emergency Feedwater Initiation and Control System-Vector D
- Engineered Safeguards Actuation System-Digital Channel 1
- Engineered Safeguards Actuation System-Digital Channel 2
- High Pressure Recirculation System-Trains A and B
- High Pressure Recirculation System-Train C

(CONTINUED)

Figure 20

SUBSYSTEMS GROUPED BY RISK SIGNIFICANCE IMPORTANCE (CONTINUED)

LOW RISK SIGNIFICANCE

- Low Pressure Injection System-Train A
- Low Pressure Injection System-Train B
- Reactor Protection System-Channel A
- Reactor Protection System-Channel B
- Reactor Protection System-Channel C
- Reactor Protection System-Channel D

Figure 21

# The Use of Risk Analysis in Evaluating Technical Specifications

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## ABSTRACT

This paper provides an overview of research conducted by Brookhaven National Laboratory (BNL) for the Division of Risk Analysis and Operations (DRAO) under the program name, Procedures for Evaluating Technical Specifications (PETS). By way of select examples, the paper highlights the rationale, methodology, and logical justification of various approaches for evaluating the risks associated with Allowed Outage Times (AOTs) and Surveillance Test Intervals (STIs) - two elements within present Technical Specifications (TS). These approaches include (a) the strategies for addressing various TS risks, (b) the levels at which TS risk can be controlled, (c) criteria for determining acceptable TS risk, and (d) measures for quantifying the risk. Also outlined are the various risk-based approaches being investigated for TS-risk evaluations.

## 1.0 INTRODUCTION

### 1.1 Purpose

The PETS Program is investigating the use of risk- and reliability-based methodologies for improving Technical Specifications. Currently, this program is examining various approaches for developing a quantitative basis for making engineering judgments for changing the Allowed Outage Time (AOT) and Surveillance Test Interval (STI) elements of TS. The purpose of this research is to provide assistance to the NRC for developing a coherent procedure for analyzing, reviewing, and evaluating modifications to current TS and, by select examples, assessing the safety impact associated with compliance to current TS.

The major products from these efforts will be two-fold. A "requirements" guide which defines analysis requirements and specifications for utilizing risk analyses to evaluate acceptable values for AOTs and STIs and acceptable risk criteria in evaluating AOT and STI risk. PETS could also assist in developing a "regulatory" guide for reviewing AOT and STI risk-based submittals. This would include guidelines on how to screen submittals to determine those for which decisions can be made without extensive evaluation and those for which a more detailed review is required.

The purpose of this paper is to highlight the efforts made thus far by the PETS Program. Major products developed thus far by PETS will be summarily referenced in this paper. However, more detail is provided herein on how PETS utilized bounding-type calculations for screening out AOT and STI importances and risks.

## 1.2 Background

By way of definition, TS can be considered as design and procedural limits that entail explicit restrictions on the operation of nuclear power plants and the maintenance of safety systems in a pre-accident condition. There is general agreement that TSs are complex and difficult to implement. NUREG-1024,[1] "Technical Specifications - Enhancing the Safety Impact," has documented past experiences which indicate that lack of guidance in TS can affect both licensing and operations. The EDO Task Group, formed in August 1983 to study the safety impact of TS, listed recommendations in NUREG-1024 which indicate that testing frequencies, surveillance tests, and action statements be reviewed to assure that they are adequately supported on a technical basis and that risk is minimized.

Industry and the NRC have embarked upon parallel and coordinated efforts to further identify problems with TS, come up with alternatives, and develop revised TS conducive to help the short- and long-term safety of nuclear power plants. The PETS Program was developed to provide research and analytical support in carrying out the EDO Task Group recommendations. Essentially this program, initiated in January 1984 at BNL, has been designed to examine approaches for developing and demonstrating a quantitative, reliability-based method for making engineering judgments in evaluating TS. The program was originally scoped to focus on two of the TS elements, viz., AOTs and STIs, and to establish procedures that employ risk-based insights for ascribing valid downtimes and testing frequencies of safety-system components. In addition, it was also structured to produce interim products to assist NRC's Office of Nuclear Reactor Regulation (NRR) in their response to Generic Issue B-56 and B-61.

During the course of program execution, as described in the PETS Program Plan[2], additional tasks were requested during FY 1985 to provide support to NRR's Technical Specification Improvement Project (TSIP) which is chartered to further identify problems with TS and to come up with rational alternatives. In order to coordinate its plans on TS improvements and interact with the NRC, industry has also formed a central group through the Atomic Industrial Forum (AIF). The PETS Program has been restructured to interface with this industrial group as well.

## 1.3 Objectives

The basic objective of this program is to develop and apply a methodology for using risk and reliability techniques to evaluate the scope and detailed requirements of plant technical specifications. Major products are the development of a "requirements" guide and a "review" guide for analyzing, reviewing, and modifying TS using risk insights as the basis.

## 1.4 Program Scope

Efforts in FY 1985 have been to develop and demonstrate an overall procedure for determining AOTs and STIs, to propose AOTs and STIs for select specific cases, to provide a requirements guide for implementing TS analysis procedures, and to develop and demonstrate risk-based guidelines for utilizing cumulative downtime allotments to control component downtime as suggested in resolution of Generic Issue B-56 and B-61. The PETS Program identified a number of safety issues which impact on the determination of AOT and STI requirements.[2,3] The significance of these documented safety issues was evaluated using the emergency coolant system of the Limerick nuclear power plant as an example.[4,5] A report describing the findings was issued for review and comment which showed how existing Probabilistic Risk Analysis (PRA) models can be used to address AOT risks as well as providing a preliminary indication as to how a technical review of licensee-submitted TS exemption requests should be conducted if the submittal employs probabilistic approaches.[5] Throughout this study, PETS identified the need to develop regulatory review strategies that should not only cover probabilistic implementation approaches (in which risk is explicitly calculated and used as the basis for exemption requests) but also should address those deterministic methods/approaches where risk considerations serve as a more implicit underlying basis for seeking changes in a specific TS.[6]

Using the PRA for Arkansas Nuclear One - Unit 1 as the basis for measuring the risk impact of testing and maintenance activities, the PETS program has also evaluated a set of the AOT and STI requirements of the plant's TS.[7] Using core-melt frequency as the risk measure and pre-defined numerical criteria that are based on the safety goal criterion for the frequency of core melt, this aspect of the study has shown not only what component's downtimes can be relaxed (increased) but also demonstrates a method for determining the risks associated with a given AOT and the risk effectiveness of a STI. This report, submitted to the TSIP group for review and comment, also provides an evaluation of the AOT and STI risks at this plant using PRA models for bounding type calculations.

In response to Generic Issue B-56 and B-61, the PETS program developed a methodology for determining cumulative downtime distribution and cumulative downtime risk distribution. The methodology, described in a letter report,[8] relies heavily on renewal theory and marked stochastic processes and is presently under review at NRR. To augment this approach and provide technical direction, further work on issues resolution of diesel tests (Generic Issue B-56) was also undertaken. This entailed sensitivity analysis for diesel generator demand test intervals and diesel generator data acquisition for separating diesel failures into those caused either by standby or demand stresses.[9,10] This aspect of the study will be used to establish diesel generator availability goals that are in concert with core-melt safety goals.

Interfacing with industry-sponsored TS programs has led the PETS program to document AOT and STI risks, calculated using PRA models, the FRANTIC code, as well as the EPRI-developed SOCRATES code.[11] Through several industry- and NRC-workshops, information was exchanged, and cooperative procedures were established. The FRANTIC and SOCRATES codes were also used to evaluate the AOT and STI risks for the emergency coolant systems of the Limerick nuclear power plant.[12] These two codes provide a more detailed analysis of AOT and STI contributions to risk. Criteria for when these more detailed codes will be required to address TS risk will be addressed by the PETS Program.

Although much has been learned regarding the use of risk and reliability methods for evaluating TS risk, this paper emphasizes how bounding-type analyses using PRA models can be employed to evaluate the risk contributions of TS and how to screen for risk significant items. Section 2 describes how PRA models can be used to evaluate AOT risks. Section 3 deals with the evaluation of test contributions to risk. The results cited are based upon the use of the IREP risk analysis for ANO-1. PRA limitations are also discussed in these sections. Section 4 summarizes these findings and indicates how the results can be used to make decisions for improving TS.

## 2.0 AOT RISK ANALYSIS

In the next two sections the risk impacts associated with the AOTs and STIs for the safety systems in the ANO-1 power plant are briefly summarized, and the effect on risk due to extensions in current requirements are investigated. Additional details can be found in References 4 and 7, where the measures of risk impacts utilized in evaluating AOT and STI requirements are further defined. The measures are chosen to obtain realistic but conservative estimates of the risk impacts. The calculations are performed using the ANO-1 PRA, undertaken as part of the Interim Reliability Evaluation Program (IREP).

### 2.1 Calculation of AOT Risk Using a PRA

The operating risk of a plant due to an AOT is the risk associated with the component being down and unavailable when needed in the event of an accident. The risk considered can be any risk characteristic such as core-melt frequency, expected fatalities, or system unavailability depending upon the level of definition used. These definitions of AOT risks are further explained in Refs. 4 and 5.

Let $r^{AOT}$ define the risk, at core melt level, from one downtime given component i is down for the AOT. The risk when the component is assumed down for the entire AOT period is given by:

$$r_i^{AOT} = C_i^+ \cdot d \quad , \tag{1}$$

where $C_i^+$ is the increased core-melt frequency when the component is down and d is the AOT. That is,

$C_i^+$ = the risk (in this example core-melt frequency) with the component assumed under AOT.

The increased risk due to the AOT is:

$$\Delta r_i^{AOT} = (C_i^+ - C_o) \cdot (AOT) \quad , \tag{2}$$

where $C_o$ is the core-melt frequency when the component is not known to be down. This definition of AOT risk is sometimes referred to as the conditional AOT risk.

The conditional AOT risk definition applies to the situation where the component is detected to be down and repair or maintenance is to be performed during the AOT period. This definition does not directly account for the frequency of maintenance or repair. The implicit assumption in this measure is that the frequency is one per year, and the component is unavailable for the entire period. In another point of view, this measure provides the incremental risk for a component unavailable for one AOT period over one year. The actual AOT risk, on the average, is usually lower than that calculated from the conditional risk measure, since, in reality, the average repair time of a component is usually lower than the AOT.

The other measure of AOT risk is the risk associated with the projected occurrences of the component being down in some time period. This risk is sometimes called the cumulative AOT risk or the projected cumulative AOT risk.

Let $R_i^{AOT}$ be the cumulative downtime risk from downtimes which can occur during a reference period. This definition of AOT risk accounts for the downtime for a given AOT as well as the number of downtime occurrences. The risk $R_i^{AOT}$ is given by:

$$R_i^{AOT} = N_i \cdot C_i^+ \cdot d$$

where $N_i$ is the numbe- of projected downtime occurrences during a defined time period, T.

The cumulative increase in risk is given by

$$\Delta R_i^{AOT} = (\omega_i T) \cdot (C_i^+ - C_o) \cdot (AOT) \qquad (3)$$

where $\omega_i$ is the maintenance frequency of component i. The maintenance frequency is usually higher than the failure rate of the component since it accounts for the maintenances to be performed for many degraded and incipient conditions of the component. The time period T is usually one year and the cumulative risk increase is measured over a year. The cumulative risk increase will be higher compared to the conditional risk increase when the number of projected downtime occurrences over a year is greater than one and vice versa.

This definition of cumulative risk increase also assumes that every time the component is taken out for service, the entire AOT period is used. As explained before, the actual AOT risk in a plant is expected to be lower since many repairs can be completed in shorter time periods, and the average time is less than the defined AOT. However, in determining AOT extensions, this definition has merit since it gives the maximum risk due to the AOT; any decision based on this measure will bound possible different scenarios.

### 2.2 Important Considerations in AOT Risk Analysis

The determination of AOT risk and its utilization in modifying current AOT requirements require a number of important considerations. An objective of the PETS Program has been to identify and evaluate the impact of these considerations in its effort to evaluate a methodology for AOT risk analysis. Ref. 5 provides a detailed analyses of various issues in AOT risk analysis.

The determination of AOT risk measures was performed utilizing the PRA of a plant. The conditional risk or core-melt frequency $C_i^+$ and $C_o$ are obtained utilizing the PRA. $C_o$ is obtained by assigning the maintenance unavailability of the component equal to zero. $C_i^+$ is obtained using the following input:

1. The component with the AOT is assumed to be down. This is equivalent to setting the component unavailability equal to 1.

2. Other components that must be reconfigured for the repair or maintenance are identified and their failure probabilities are modified to represent the reconfigured state.

3. Other components or system trains that are required to be operable are identified, and they are assumed not to be in maintenance, since that will violate the TS limitations.

The representation of reconfigurations and operability requirements of other components during maintenance requires an evaluation of the system fault tree models. This evaluation involves the following considerations - 1) changes in component unavailability due to reconfigurations, 2) changes or elimination of certain human errors considered in the original fault tree, 3) inclusion of additional human errors resulting from failure to reconfigure, and 4) changes in the dependent human failures due to the effect of reconfiguration. In addition, the requirement of availability of redundant trains and components implies that they are not in maintenance and accordingly, their maintenance contribution to unavailability is neglected for conditional AOT risk calculations. The remainder of the input is the same as that used for PRA calculations. Using the above inputs, the calculation of $C_i^+$ at core-melt level incorporates system and component interactions.

The relative impact of the above aspects in AOT risk analysis depends upon the component being maintained and the associated requirement of reconfiguration. These issues have been studied within the PETS program and Ref. 5 provides select examples. In addition, another important aspect in AOT risk is the testing of redundant trains before a repair is begun. Such strategies will, in instances, result in unavailabilities lower than the average unavailabilities used in PRA analyses and will impact the AOT risk analysis. This aspect is not currently included in the analysis, but a detailed analysis is planned in the PETS program.

The calculation of AOT risk using the input requirements defined above requires certain cautions.[5] When a component unavailability is equal to 1 in minimal cut sets, the resulting cut sets can no longer be minimal and may need to be transformed to the minimal form. PRAs provide the minimal cut sets truncated at a certain value ($10^{-6}$ or $10^{-8}$), otherwise the number of minimal cut sets at each level may become unmanageably large. If minimal cut sets are used to calculate the risk during AOT, care should be taken that cut sets containing the AOT component are not already truncated. Many cut sets of low magnitude containing the AOT component may become dominant contributors when the component unavailability is equated to 1. Failure to account for the cut sets containing the AOT component will result in underestimation of the AOT risk.

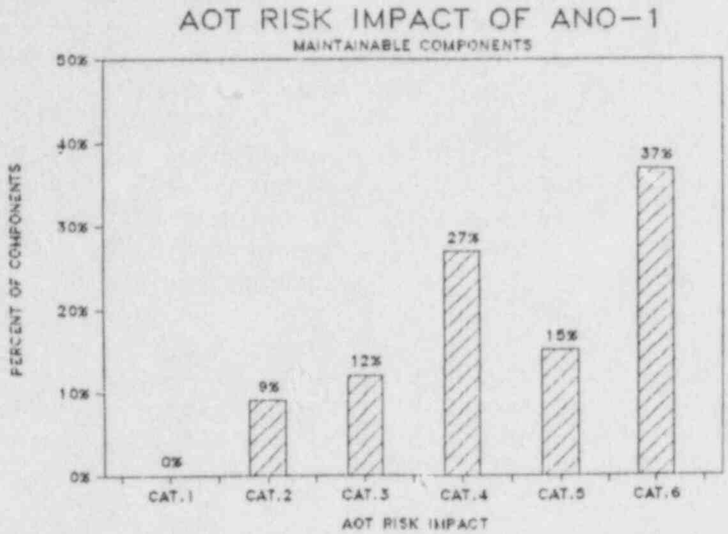
In this paper the AOT risk is measured at core-melt frequency level, but AOT risk can be measured at any other level. For example, system unavailability or function unavailability can also be used as an appropriate level of analysis if proper attention is paid to system and component interactions.[5]

Another important aspect in modifying AOTs, based on risk analyses, is the choice of numerical criteria. Currently, no criteria exist that can be used to determine the acceptable level of risk due to AOT extensions. The PETS Program studied implications of different forms of criteria or acceptability guidelines and is working towards the development of acceptable criteria for AOT modifications. The methodology of AOT risk analysis, the risk measure used, the data base used for calculation, and the choice of criteria are related. For example, using a conservative data base associated with a criterion based on a risk measure of factor increase in baseline risk due to change in AOT, will result in nonconservative AOTs. These various interrelations, along with the consideration of PRA uncertainties, will be evaluated to make recommendations regarding numerical criteria for AOT modification.

### 2.3 Evaluation of AOT Risk for ANO-1

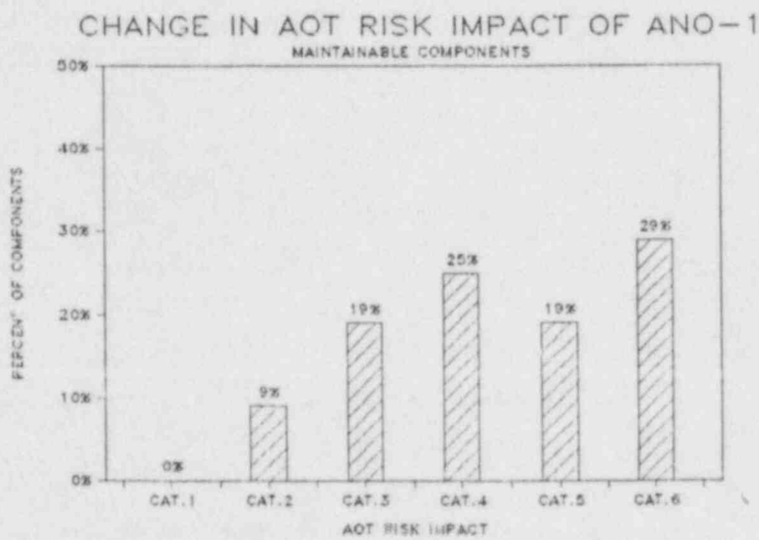
Figure 1 shows the current risk profiles of AOT requirements at ANO-1 nuclear power plant based on projected cumulative AOT risk. The figure indicates that the projected cumulative risk is below $10^{-7}$ for 79% of the maintainable components. This clearly shows a wide disparity in current AOT requirements based on risk arguments.

In this study, the impact of changes in AOT requirements was studied to analyze how the AOT risk profile can be altered. Obviously, components with low AOT risk impacts are candidates for extensions from the point of view of risk analysis. The AOTs of components with risk impacts below $10^{-7}$ were increased by a factor of two and the resulting risk profile is presented in Figure 2. For this calculation, other conditions (in terms of operability requirements of alternate trains and components) were maintained unchanged. The total cumulative risk increase due to the extension of AOTs is approximately $1 \times 10^{-6}$, i.e., about 0.25% of the baseline risk due to core-melt frequency. Comparing these two figures indicates that the impact on the risk profile is also minimal. As expected, there is a slight shift towards high risk categories; nevertheless, the




CAT 1 $10^{-5}$ TO $10^{-4}$
CAT 2 $10^{-6}$ TO $10^{-5}$
CAT 3 $10^{-7}$ TO $10^{-8}$
CAT 4 $10^{-8}$ TO $10^{-7}$
CAT 5 $10^{-9}$ TO $10^{-8}$
CAT 6 LESS THAN $10^{-9}$

Figure 1. Cumulative AOT Risk Impact of ANO-1 Maintainable Components.




CAT 1 $10^{-5}$ TO $10^{-4}$
CAT 2 $10^{-6}$ TO $10^{-5}$
CAT 3 $10^{-7}$ TO $10^{-6}$
CAT 4 $10^{-8}$ TO $10^{-7}$
CAT 5 LESS THAN $10^{-8}$

Figure 2. Cumulative AOT Risk of ANO-1 Maintainable Components for Factor of Two Increase in AOTs of Components with Risk Impact Below $10^{-7}$.

risk impacts of 73% of the components are still below $10^{-7}$. In such an approach to AOT extensions, the effect on high risk categories is minimal.

### 2.4 Safety Implications of AOT Modifications

The results of the risk impact of the ANO-1 AOT requirements provide evidence that many of the AOT requirements are risk-unimportant. The expected frequency of maintenance of many of these components is low, and a large increase in their AOTs will not impose undue risk.

The extensions in AOTs will have both near- and long-term safety implications and will allow sufficient time to complete repair, thus reducing the number of unscheduled plant shutdowns due to TS violations. This avoids the risk associated with transfer of operational modes of a power plant and also limits situations where transfer to shutdown mode is a riskier option. In the long run, extended AOTs will reduce the "band-aid" fixes resulting in lower maintenance frequency for components. Thus, besides the increase in plant availability due to AOT extensions, the associated safety benefits are also attractive.

From a risk control point of view, an argument can be made that TS should focus on components and conditions that are significant contributors to risk due to outages. Risk and reliability analyses provide evidence that critical combinations of components if simultaneously unavailable may cause a large increase in risk.[5] Current technical specifications do not always control these critical combinations of components which are identifiable through risk analysis. In considering any modification to technical specifications, assurances must be available that outage of safety significant critical combination of components do not overlap.

## 3.0 STI RISK ANALYSIS

### 3.1 Calculation of STI Risk Using a PRA

The risk impact of a component in test is the expected gain in risk aversion due to the ability of the test to detect the failure that may have occurred during the standby period and may otherwise have gone undetected. The risk impact or the risk benefit of testing can be interpreted in a probabilistic sense to be the difference in the expected risk before and after the test. Ref. 6 provides a detailed derivation of the risk impact of a surveillance test and it can be represented as

$$r_{STI} = q_t [R^+ - R^-] \quad , \qquad (4)$$

where $r_{STI}$ is the risk impact of a surveillance test on a component, and $q_t$ is the component unavailability with current STI. $R^+$ signifies the risk when the component is known to have failed, and $R^-$ is the risk when the component is assumed operable.

The above definition of the risk impact of a surveillance test assumes that both the demand and time-related failure modes are detectable and correctable by the test. Surveillance tests control the contributions to failures during the test interval, and accordingly the risk impact of a surveillance test should be developed based on time-related failure mode only. The use of this definition, however, requires a proper partition of the failure data into time-related and demand-related failure modes. Since in actuality many of the failures detected through demand testing have the time-related failure cause, an evaluation of each failure is necessary to develop the data base. Detailed component-specific data to this level are not presently available, and use of only the time-related failure rate $\lambda$, as currently used in PRAs, will result in an underestimation of the risk impacts of the surveillance tests. A conservative approximation to Eq. (4) is

$$r_{STI} = \lambda' t[R^+ - R^-] \quad , \tag{5}$$

considering all the failures as per unit time contributions. Here, $q_t$ is represented by $\lambda' t$,

$$q_t = \lambda' t = q_o + \lambda t \quad ,$$

where $q_o$ is the demand related failure probability, t is the exposure time, and $\lambda'$ is the assumed failure rate considering all failures to be time related. This approach is conservative since it calculates the largest risk impact associated with the test.

Many surveillance tests are associated with a downtime, i.e., the component is not unavailable and cannot be returned to the emergency safety position if a demand were to occur during the test. If contribution from test downtime is included, the risk impact, $r_{TT}$, is represented in the form

$$r_{TT} = q_t[R^+ - R^-] - \frac{\tau}{t} \cdot R^+ \quad ,$$

where $\tau$ is the duration of the test. In most instances, $\tau$ is much smaller than t and the effect of the term is negligible. If test intervals are increased, the effect of the term will be even smaller.

The above discussion on the risk benefit of testing does not include the possibility of degradation due to testing, the effect of component wear-out and test-caused transients resulting in the possibility of unscheduled shutdowns. Incorporation of those parameters will require much more complex evaluations and is facilitated by the use of computer codes like FRANTIC. An evaluation of impact of these parameters in surveillance test requirements is being performed as a part of the PETS program. The analysis presented above for evaluating the risk benefit of surveillance tests is an approximation to more complex models and will be useful for screening purposes. However, the determination of the necessary integral tests to be performed in a plant and the test strategies from risk considerations will require more complex evaluations.

The risk impact or the risk benefit of testing requirements is dependent on the test interval and changes in the impact due to alternate testing requirements can be evaluated through Eq. (4). When the test interval is increased, the increase in the risk impact is obtained as

$$\Delta r_{STI} = (q_{t_1} - q_t)\cdot(R^+ - R^-) \quad ,$$

where $q_{t_1}$ is the unavailability with the increased test interval $t_1$, and $q_t$ is the unavailability with the current test interval t. In terms of failure rates this becomes

$$\Delta r_{STI} = \lambda(t_1 - t)(R^+ - R^-) \ ; \text{ for } \lambda t,\ \lambda t_1 < 0.1 \quad ,$$

where $t_1$ is the new test interval. The expression has two implicit assumptions. One is that the failure rate remains unchanged with the change in the test interval. There is strong reason to believe that unless the component is experiencing wear-out and that the new test interval does not violate manufacturer recommendations, the hazard rate ($\lambda$) will remain constant. The other assumption is that the demand related failure probability also remains constant with the change in the test interval. Arguments have been made that demand testing of a component in effect causes degradation, and that an increase in the test interval (or decrease in the number of demand tests) will actually decrease the demand failure probability. This assumption introduces a slight conservatism in the above expression. In addition, when test intervals are increased $\lambda t_1$ may become greater than 0.1, and $q_{t_1}$ should be expressed as

$$q_1 = q_o + (1 - e^{-\lambda t_1}) \quad .$$

The discussion presented thus far relates to the risk impact of a surveillance test on a single component. In performing a surveillance test a number of components are invariably tested simultaneously. Thus, the surveillance test will detect failures in all the components in the path, and the risk impact of the surveillance test should account for all the components and failure modes tested. For n components listed in a given test,

$$R_{STI} = \sum_{i=1}^{n} q_i \cdot (R_i^+ - R_i^-) \quad ,$$

where $q_i$ is the unavailability for the ith component,

$R_i^+$ is the risk associated with the ith component assumed down, and

$R_i^-$ is the risk associated with the ith component assumed up.

The incremental risk impact for the change in surveillance test interval will then be given by:

$$\Delta R_{STI} = (t_1 - t) \sum_{i=1}^{n} \lambda_i (R_i^+ - R_i^-) \quad ,$$

where $\lambda_i$ is the failure rate of the ith component. The difference in test intervals is outside the summation by definition of simultaneously tested components.

### 3.2 Important Considerations in STI Risk Analysis

As discussed, the determination of STI risk using currently available PRA will have a number of assumptions along with the assumptions in PRA. A detailed evaluation using FRANTIC and SOCRATES Computer Codes,[12] being performed within the PETS program, shows the influence of various parameters in STI risk evaluation. The important considerations in STI risk analysis include the incorporation of appropriate test strategy, the effect of test-caused degradation, and the partitioning of failure data into demand- and time-related contributions.

The determination of STI risk measures using current PRAs requires the evaluation of the conditional risk assuming the tested components to be up ($R^-$) or down ($R^+$). When the risk is evaluated at the core-melt level, $R^+$ is obtained by calculating the core-melt frequency by assigning an unavailability equal to 1 to the tested component. $R^-$ is similarly obtained by assigning an unavailability equal to zero to the component. Similar calculations are performed for each of the components tested in the surveillance test.

The calculation of $R^+$, when the component unavailability is equal to 1, requires similar care as that used for calculating the AOT risk. Namely, the cut sets generated may need to be transformed to the minimal form, and the minimal cut sets used for the evaluation must not be truncated to eliminate cut sets containing the component in question.

Similar to AOT risk analysis, STI modification using risk-based analysis will require numerical criteria. The discussion of criteria on AOT modifications (Section 2.2) is also applicable for STI modifications, and the numerical criteria developed in both instances should be consistent.

### 3.3 Evaluation of STI Risk for ANO-1

The risk impact due to a surveillance test is the risk averted through detection of any standby failure. The surveillance tests identified in the safety system are analyzed with respect to their risk impacts; detailed results are presented in Reference 6.

The results of the analysis show a trend similar to that observed for AOT requirements. A significant number of tests have minimal impact on risk. Fifty-three percent of the surveillance tests have risk impacts below $10^{-7}$; 14% have an impact higher than $10^{-5}$.

Figure 3 shows the risk profile of current surveillance test requirements. Changes in STIs were studied by increasing the interval requirements of tests having a risk impact below $10^{-7}$. Figure 4 shows the risk profile of surveillance tests with test intervals increased by a factor of two for those tests with risk impacts originally below $10^{-7}$. The effect on the risk profile is minimal except for rearrangements in the third and fourth categories. The incremental risk impact due to such an extension is of the order of $1 \times 10^{-6}$, i.e., approximately 0.25% of the baseline risk due to core-melt frequency. Still 50% of the tests will have risk impacts below $10^{-7}$. The risk impacts of many of the surveillance tests are so small that a much larger extension can easily be granted from risk considerations.

### 3.4 Safety Implications of STI Modifications

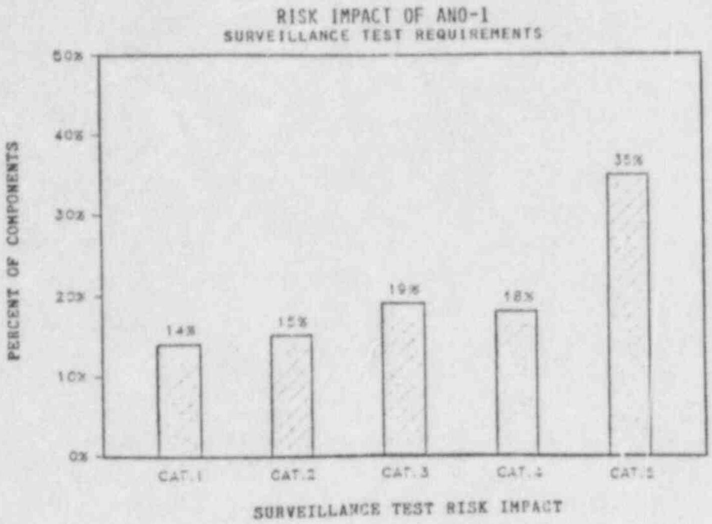
Risk analyses of surveillance test requirements show that a significant increase in the test intervals for a large fraction of the STIs will have negligible incremental risk impact. Risk impact measures used in this analysis, as previously discussed, are conservative, i.e., incorporation of additional parameters influencing STIs will show even lower risk impact. However, the risk impact for a fraction of the tests is significant, and assurances are necessary that these tests are performed to detect any failures occurring in the standby period. In addition, manufacturer recommended test intervals considered necessary to maintain the integrity of the component should not be violated.

The increase in the STI will reduce the number of tests to be performed in a plant, and argument is made that this will allow the operating personnel to concentrate on important safety aspects. Also, the reduction in the number of test requirements on a component is expected to reduce the test-caused degradation of components, if any exists. The chances of test-caused transients resulting in unscheduled shutdowns will also decrease. The combined effect of all of these will contribute to the long-term safety of the plant.

The approach to modify STIs may take different shapes. The results of this study have indicated that integral tests, i.e., tests of systems or system trains detecting failure modes of a group of components, are more risk effective. Risk insights can be used to develop fewer integral tests to control the risk, and the remaining test requirements can be moved to some form of supplemental specifications. Removing all STI requirements to supplemental control where the test interval of risk important integral tests are significantly increased may require an alternate form of risk control activity. In various cases, conditional monitoring of risk-important failure modes of selected components can be more effective than current surveillance test requirements. The candidate components for condition monitoring can be identified from the results of this or similar studies.

## 4.0 SUMMARY AND CONCLUSIONS

The paper presents a brief overview of the activities being conducted under the PETS Program for DRAO at BNL for improving TSs. Efforts under the PETS Program have concentrated on two aspects of TS, namely, AOT and STI. The results of the program provide the basis for the following conclusions on various aspects of technical specification issues.




CAT 1 $10^{-5}$ TO $10^{-4}$
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CAT 3 $10^{-7}$ TO $10^{-6}$
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CAT 5 LESS THAN $10^{-8}$

Figure 3. Risk Impact of ANO-1 Surveillance Test Requirements.

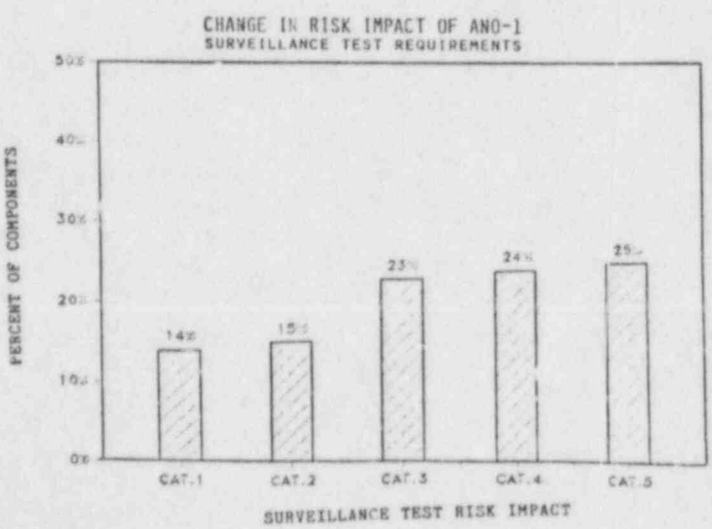


Figure 4. Change in Risk Impact of Surveillance Test Requirements with a Factor of Two Increase in the STIs of Tests with Risk Impact Below $10^{-7}$.

## 4.1 Risk Implications of Current Requirements

The risk impacts of AOT and STI requirements vary widely on the risk scale. A significant portion of these requirements have minimal risk impacts. Approximately fifty-two percent of the risk impacts of both AOT and STI requirements studied in the ANO-1 nuclear plant are below $10^{-7}$. These requirements are therefore not an effective means of control from a risk point of view.

## 4.2 Bases for Technical Specification Requirements

Many items identified in technical specifications, particularly those associated with LCO requirements, do not have valid technical bases. In many areas of technical specifications the risk based methodology developed in the PETS Program and those being studied under different research programs can provide the means to establish valid technical bases. A consistent risk basis for specifications will result in clarity of purpose and better compliance. The process of extensions and changes in the specifications can also be streamlined.

## 4.3 Validity of Action Statements

The action statements in current specifications appear unnecessarily restrictive and potentially adverse to long-term safety of the plant. The results of this study indicate that the risk impact of many of the requirements is minimal and extensions to or relaxation of AOTs will also impose minimal incremental risks. Under these circumstances, requirements for changes in the operational mode of the plant due to infrequent violation of an AOT are unnecessary and possibly introduce more risk due to transfer to shutdown mode and return to operational mode. To enhance the long-term safety of the plant, these restrictive action statements can be modified to requirements for problem detection and correction without undue implication on risk. Risk and reliability based insights can form the basis of such modifications.

## 4.4 Changes in AOTs and STIs

Based on the risk implications of changes in AOTs and STIs of risk-unimportant components, it is quite evident that such extensions can be granted without affecting the risk from the plants. Extensions in AOTs will allow for the adequate repair of components, should reduce the expected frequency of outages, and should reduce unscheduled shutdowns. Extensions in STIs can be granted with minimal implication on risk and concomitantly reduce the burden of the operational staff so that they can focus on more safety significant activities. Reducing the number of tests will also reduce unscheduled plant shutdowns resulting from test-caused transients. Changing these requirements to more acceptable limits will result in the reduction of one-time extensions and exemption requests.

## 4.5 Improvement to Current Technical Specifications

In this study, the evaluation of AOT and STI requirements of a plant has identified many inconsistencies in current technical specifications. Similar efforts can be extended to certain other areas of technical specifications. The risk-based analysis can be used to significantly reduce the number of surveillance test requirements, and more integral but fewer surveillance tests can be defined that will be adequate to maintain the risk level of the plant. The AOTs can be defined from a risk perspective allowing adequate time for repair, and the action statements can be modified to address both the short- and the long-term safety of the plant.

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# Regulatory Considerations of Severe Accidents

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## Abstract

The U.S. Nuclear Regulatory Commission recently issued a policy statement on severe accidents. Implementation of the policy statement for new plant applications includes issuance of guidance on the role probabilistic risk assessments (PRAs) are to play in severe accident analysis and decision making, and formulation of new performance criteria for containment systems, if needed. The adequacy of existing plants will be evaluated through the analysis of selected reference plants and examination of individual plants for severe accident vulnerabilities. Knowledge and experience gained from the plant analysis and the severe accident research program will be used to review and modify rules and regulatory practices related to severe accidents. PRAs are expected to be an important part of the implementation program. Information available from PRAs will be used to the fullest extent possible, but with appropriate consideration of the specific PRA's quality and of the uncertainty associated with the PRA.

## The Commission's Policy Statement on Severe Accidents

On August 8, 1985, the U.S. Nuclear Regulatory Commission issued a policy statement on severe accidents (Reference 1). The policy statement provides criteria and procedural requirements for the licensing of new plants, and sets goals and a schedule for the systematic examination of existing plants. On the basis of available information the Commission concluded that existing plants pose no undue risk to the public and the Commission sees no present basis for immediate action on generic rulemaking or other regulatory changes for these plants because of severe accident risk. Thus, the Commission withdrew the advanced notice of proposed rulemaking on Severe Accident Design Criteria published on October 2, 1980 (Reference 2). However, the Commission emphasized that a systematic examination of existing plants are needed, encouraged the development of new designs that might realize safety benefits and stated that the Commission intends to take all reasonable steps to reduce the chances of occurrence of a severe accident and to mitigate the consequences of such an accident, should one occur.

With respect to new plant applications the Commission specified acceptance criteria and procedural requirements, which include completion of a Probabilistic Risk Assessment (PRA) and consideration of the severe accident vulnerabilities the PPA exposes. The policy statement also specifies two action items which will help future applicants in the design and safety analysis of their plants. First, within 18 months of the publication of the policy statement, the staff will issue guidance on the form, purpose and role that PRAs are to play in severe accident analysis and decisionmaking for both existing and future plant designs and what minimum criteria of adequacy PRAs should meet. The PRA guidance will describe the appropriate combination of deterministic and probabilistic considerations as a basis for severe accident decisions. Second, a clarification of containment performance expectations will be made including a decision on whether to establish new performance criteria for containment systems, and, if so, what these should be.

For existing plants the Commission plans to formulate an integrated, systematic approach to an examination of each nuclear power plant now operating or under construction for possible significant risk contributors that might be plant specific and might be missed absent a systematic search. The examination will pay specific attention to containment performance in striking a balance between accident prevention and consequence mitigation. The systematic approach will be formulated during a two year period following issuance of the policy statement and will include the development of guidelines and procedural criteria, with an expectation that such an approach will be implemented by licensees of the remaining operating reactors not yet systematically analyzed in an equivalent or superior manner. As part of the examination, individual plant vulnerabilities will be identified together with the most cost-effective options for reducing vulnerabilities. The Commission's backfit policy will be used to decide which options need to be implemented. Any generic design changes that are identified as necessary for public health and safety will be required through rulemaking.

Implementation of the Policy Statement

A program is presently being developed for the implementation of the policy statement. The goal of the program is to assure that severe accident vulnerabilities, if such vulnerabilities exist in plants or plant designs, will be found and will be corrected. The approach, followed, will try to take full advantage of similarities among plants of a given type. Thus, plant type dependent vulnerabilities will be identified in the detailed review of selected plants (reference plants), while plant specific weaknesses will be the subject of a limited plant examination performed on each individual plant. The new knowledge gained from the severe accident effort together with the specific vulnerabilities found in the plant reviews will be used to update and modify rules and regulatory practices, as necessary. The following sections discuss the key elements of the program, namely reference plant analyses, individual plant examinations, and changes in rules and regulatory practices.

Reference Plant Analyses

With one exception, all U.S. nuclear power plants (operating and under construction) are light water reactors of either the pressurized water reactor (PWR) or boiling water reactor (BWR) types. However, there are numerous design differences among each of these reactor types which evolved during the past twenty years, the formative years of nuclear power plant design. Recognizing the important role containments play in the mitigation of severe accidents, and the fact that there are three basic containment designs for both U.S. PWRs and BWRs, we elected six reference plants, for detailed analysis as shown on Figure 1.

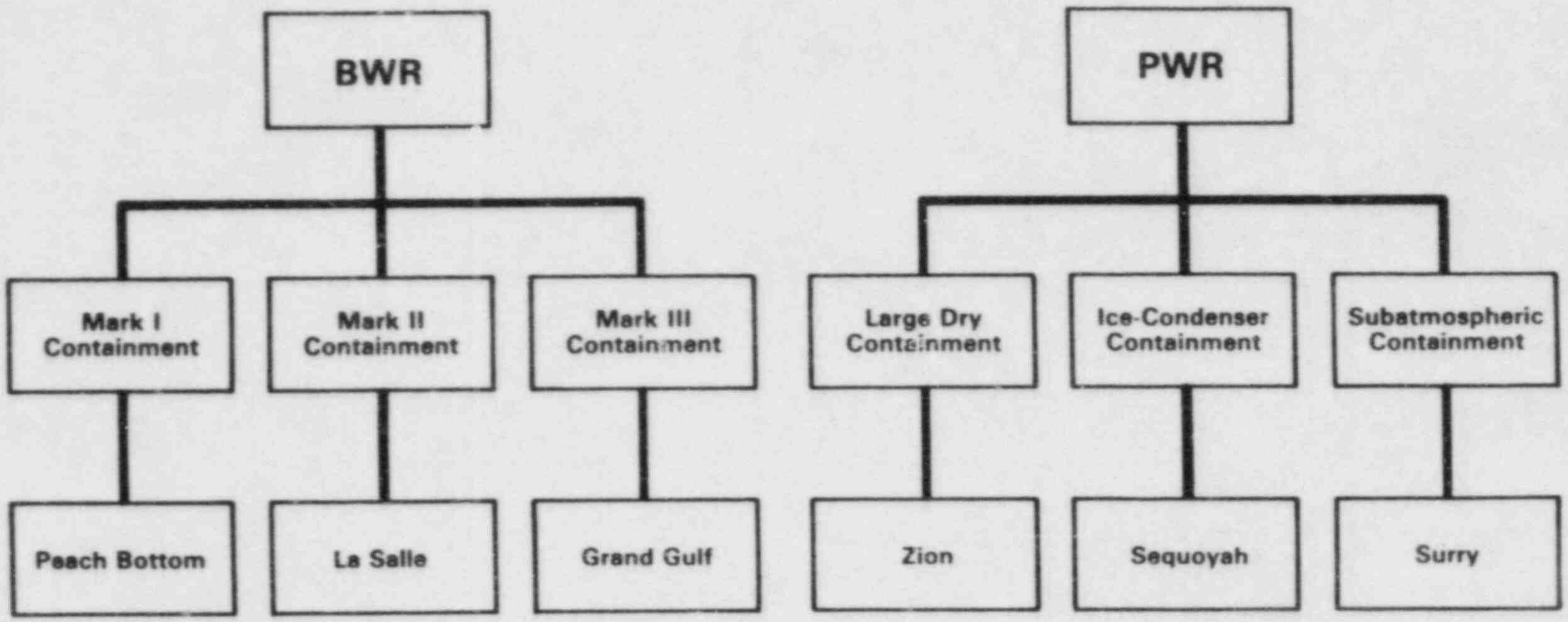
Each reference plant selected has already had a PRA performed. Some of these PRAs are quite old (ten years), some are of more recent vintage. The reference plant analysis builds on the existing PRA, updates it as needed, and completes the identification and evaluation of all potentially important severe accident sequences. Information available from the severe accident research program on important phenomena, like core melt progression, fission product and hydrogen release, containment loading and performance and fission product chemistry, were used to enhance our understanding of severe accident progression and develop improved computational capability for the reference plant analyses. The results of the analyses will be presented in terms of predicted core damage frequency, fission product release from the containment (including timing of the release) and public risk.

Probably the most difficult task of severe accident analysis is the assessment of uncertainties. While most severe accidents result in core melt and significant fission product release, it is not clear that these processes will progress similarly in each case. Neither is it clear that progression would be the same if the same event occurs again at a similar plant. Our ability to model these processes introduces further uncertainties. Nevertheless, it is our goal to establish realistic uncertainties associated with the analysis of each reference plant and display them in terms of each of the results mentioned above. Furthermore, major contributors to uncertainties will be identified and their effect will be traced through the analysis.

Parallel with the NRC effort, IDCOR (Industry Degraded Core Rulemaking Program) on behalf of the nuclear industry has also analyzed four of the six

FIGURE 1

# Reference Plants



reference plants. The IDCOR analysis and its results have already been reported (Reference 3) and presented to NRC. Based on the IDCOR presentations and on understanding gained from the severe accident research program, nineteen technical issues were identified which were treated differently by the two parties and which were judged to have a significant effect on the outcome of severe accidents. These issues are listed in Table I. NRC and IDCOR have discussed the outstanding issues, agreed on an approach to resolution and are currently pursuing resolution. A report providing most of IDCOR's contribution toward resolution of these issues has recently been published (Reference 4).

A shortcoming of both the NRC and IDCOR analyses is that external events (seismic events, high winds, fires, floods, etc.) are not addressed. Three potential approaches are under consideration for external events. First, out of the six reference plants, four do not have PRAs with external events. However, there are more than a dozen PRAs with external events for other plants. This approach would use information from existing PRAs and transfer the knowledge to the reference plants in order to complete the reference plant analyses. The second approach would be to perform a limited external event study for the remaining four reference plants. A simplified risk assessment methodology currently under development in the research program would be used. The third approach is to use available knowledge and experience to identify specific plant vulnerabilities to external events. Each plant would then be checked against these vulnerabilities and compared against an acceptance standard. So far no decision has been made on the treatment of external events, this issue is currently under consideration.

Individual Plant Examination

The purpose of the reference plant analysis and review is twofold: (1) evaluate the performance of reference plants with respect to severe accidents, and (2) based on the experience gained from the evaluation of the reference plants, develop guidelines and procedural criteria for the systematic examination of individual plants. The actual examination of the individual plants will be performed by the licensees. The licensee's involvement in this last step is an important part of the program. The organization responsible for operation of the plant should be keenly aware of complications that could arise in case of severe accidents and of potential mitigating actions, including the use of systems and equipment normally not considered safety related.

Once, one plant of a given design type, the reference plant, has been analyzed in detail, many of the findings can be interpreted to other plants of the same design. It will not be necessary to repeat the detailed analysis for each individual plant. The purpose of the guidelines is to specify under what conditions can an individual plant adopt the reference plant analysis, describe the extent of plant specific examination licensees

TABLE I
NRC/IDCOR Technical Issues

I. Core Heatup Stage

1. Fission Product Release prior to Vessel Failure
2. Recirculation of Coolant in the Reactor Vessel
3. Release Model for Control Rod Materials
4. Fission Product & Aerosol Deposition in the Reactor Coolant System

II. Melt Progression and Fuel Relocation Stage

5. Modeling of In-Vessel $H_2$ production
6. Core Slump, Core Collapse and Reactor Vessel Failure Models
7. Alpha Mode Containment Failure by In-Vessel Steam Explosions

III. Ex-Vessel State

8. Direct Heating of Containment by Ejected Core Material
9. Ex-Vessel Fission Product Release
10. Ex-Vessel Heat Transfer Models from Molten Core to Concrete/Containment
11. Revaporization of Fission Products in the Upper Plenum
12. Deposition Model for Fission Products in Containment

13A. Amount and Timing of Suppression Pool Bypass

13B. Retention of Fission Products in Ice Beds

14. Modeling of Emergency Response
15. Containment Performance
16. Secondary Containment Performance
17. Hydrogen Ignition and Burning
18. Essential Equipment Performance

need to conduct, and identify acceptable methodology for the search for plant specific vulnerabilities. The results of the plant specific examination will have to be measured against standards or criteria to judge acceptance. These criteria will be published together with the guidelines.

IDCOR is currently developing simplified methodology for individual plant examination. The methodology will take full advantage of the reference plant analysis and will focus the licensee's attention on plant to plant variations of important contributors to severe accident risk. The methodology is expected to provide for limited fault tree analysis with emphasis on variations in the balance of plant design. It will utilize event trees to take into account variations in systems design and operating procedures. It will check on important characteristics of containment design and inputs to consequence analysis. The methodology should also provide means to transfer information that was generated for the reference plants over to other plants of the same type. For example, assessment of uncertainties is a major effort for the reference plants. Simple procedures should be available to licensees for applying the reference plant uncertainties to individual plants with appropriate modifications. Similarly, equipment needed to mitigate the consequences of severe accidents are frequently common within plants of the same type. Information generated on equipment performance under severe accident conditions should, also, be transferable. NRC will review the IDCOR methodology, and will issue its evaluation and approval concurrent with the issuance of the guidelines and criteria mentioned above.

Following issuance of the guidelines, criteria and approved methodology, licensees will be requested to examine their plants for severe accident vulnerabilities. If plant specific shortcomings are found licensees will identify and evaluate potential corrective actions like changes in procedures or design modifications. The most cost effective options among the identified corrective actions will be evaluated and a decision will be reached consistent with the cost-effectiveness criteria of the Commission's backfit policy as to which option or set of options are justifiable and required to be implemented.

Changes in Rules and Regulatory Practices

Twenty years ago, when regulations were established for the licensing of nuclear power reactors only a limited understanding of severe accidents was available. Predictive capability to analyze severe accidents was not yet developed and there was no operating experience with power reactors. A relatively simple approach toward potential accidents was adopted. Plant safety systems were required to be designed for events which were judged to occur, possibly during the combined life of current generation nuclear plants (design basis accidents). Accidents not expected to happen in nuclear plants, for example, because postulation of these events would require multiple failures, were judged to be incredible and were not included in the design basis. Major core damage accidents (severe accidents) fell in this category. Nevertheless, consistent with the defense-in-depth approach followed for protection of the public, the adequacy of limits on containment leak rate and on site boundary were based on the assumption that major core damage took place and a postulated amount of radioactivity (Reference 5) was released from the core.

As operating experience accumulated and predictive capability was developed (Referemce 6), changes were made to the regulations to protect against selected events resulting from multiple failures and to take advantage of the more realistic calculational approach. Most of the severe accident research, however, took place in the past five years. A better understanding of events that could lead to major core damage is now available. Fission product release, chemistry and transport has been studied in detail. This is an opportune time to re-examine the regulatory approach toward severe accidents and see if the knowledge available today is sufficient to permit treatment of severe accidents in a more consistent and more realistic manner.

There are three main issues that need to be addressed before actual changes in regulations on regulatory practices could take place. First, a more realistic regulatory framework needs to be formulated for severe accidents. Second, appropriate but simple means of eliminating severe accident vulnerabilities from plant design and operation must be found. And finally, practical, realistic source terms need to be developed to replace the current inconsistent requirements.

In general, the current regulatory approach uses conditions, such as containment loads or environmental conditions for equipment qualifications, derived from design basis accidents. There is one exception: radioactive material release or source term. Since the plant is designed to accommodate design basis accidents, these accidents do not result in significant radioactive material release. Instead, releases representative of severe accidents are incorporated in the design basis. The result is a set of regulations that treats design basis events in an overly conservative manner relative to source terms, but falls short of assuring safe plant performance in case of severe accidents. It is a purpose of the implementation program to develop and propose a new regulatory framework for severe accidents which could be conducive toward the elimination of severe accident vulnerabilities and at the same time would permit the use of consistent, realistic source terms. For the purpose of illustration, a potential approach could be to identify selected severe accidents for each plant type and include the evaluation of these events in the safety assessment of the plant. The selection process would eliminate very unlikely events from consideration, it would purposely cover representative types of severe accidents and would simplify the effort needed for plant review and source term calculations.

When the regulatory framework has been selected, and guidance has been developed for the systematic examination of existing plants, the guidance will be reviewed for potential incorporation in the regulations or regulatory practices. Special attention will be payed to the assessment of the need for establishing severe accident prevention guidelines and containment performance criteria. If it is shown that the protection of public health and safety justifys action then such guidelines and criteria will be formulated and incorporated in regulatory practices either through rulemaking or by other appropriate steps (revisions of regulatory guides and/or revisions in the standard review plan).

The most frequently mentioned changes to the regulations relate to the source term. A large portion of the source term research is now complete. NRC has issued a report (Referemce 7) describing a set of computer codes now available and capable of calculating source terms for severe accidents. The American Nuclear Society issued its evaluation (Reference 8). With this information available the question we are now frequently facing is: when are we going to change source term related regulations? - As it is summarized in Figure 2, three essential elements are needed for rulemaking: methodology, regulatory framework and practical new source terms. The first one, the methodology is almost in place. There is only one key factor missing: quantification of uncertainties. So far neither NRC, nor the industry has assessed and quantified the uncertainties associated with the quantity, timing and composition of fission product release from severe accidents. The NRC program on this subject is now underway and scheduled to be completed by the summer of 1986. The IDCOR program is about ready to start.

The second element, the regulatory framework is not in place yet. No recommendation has been made from the industry in this regard. Neither did NRC put forward any proposals. This issue is expected to receive increased attention in the coming year. Finally, the third element, the development of new source terms is in the discussion stage. It is becoming more and more obvious that the various regulatory uses of source terms require different forms of source terms. For example, the resolution of safety issues related to severe accidents is well suited to detailed source term calculations. However, equipment qualification needs a simple source term, easily available to equipment manufacturers and applicable to all plant types. We foresee, at most, three forms of source terms: (1) detailed source term calculations for individual plants; (2) use of tables or procedures applicable to plant types; and (3) a simple bounding source term applicable to all plants. New rules should be formulated in a manner that applicants and licensees could opt to any one of the established forms for each use of the source term.

In preparation for the forthcoming changes in source term related requirements, rules and regulatory practices were reviewed by NRC. Twelve areas (see Table II) were identified which currently use source terms and could

FIGURE 2

# Information Needed to Initiate Source Term Related Changes

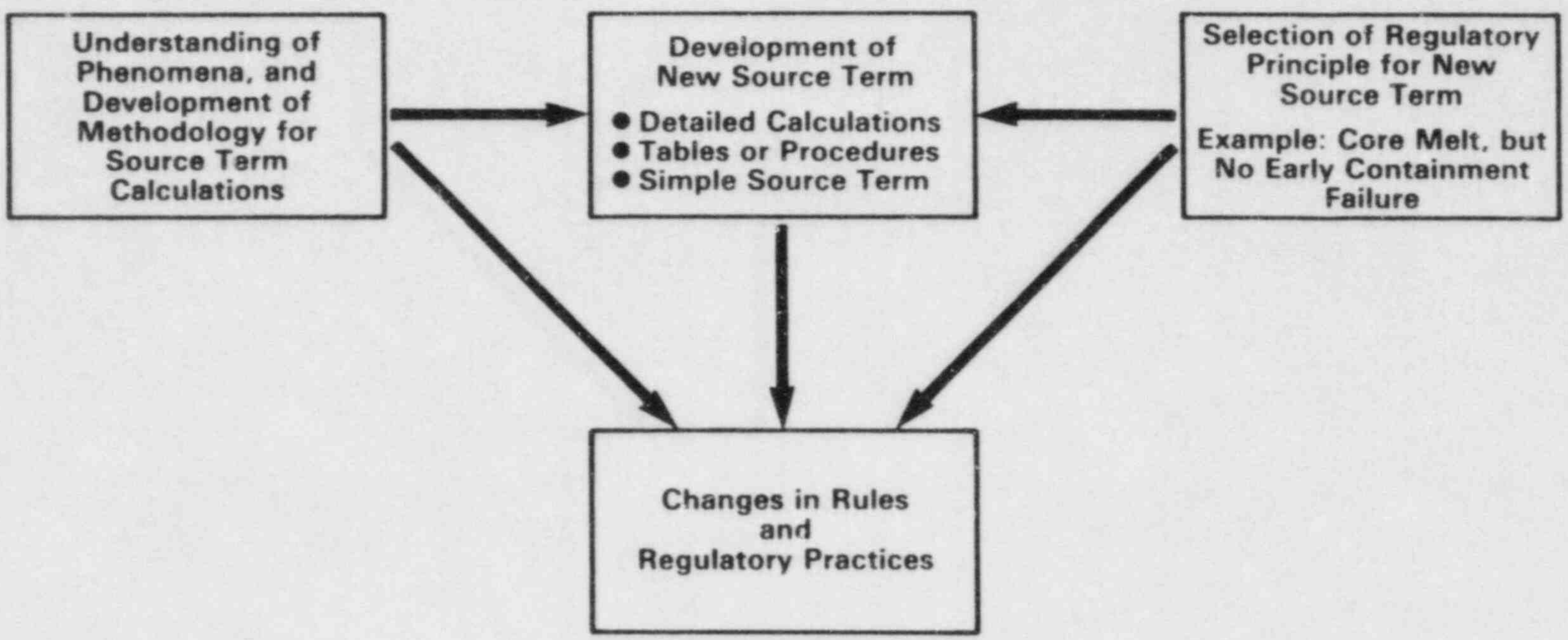


TABLE II

Potential Changes in Rules and Regulatory Practices

I. Short Term

1. Reconsideration of Containment Spray Additives (PWRs)
2. Credit for Fission Product Scrubbing in Suppression Pools (BWRs)
3. Revised Treatment of Severe Accidents in Near-Term Environmental Impact Statements

II. Intermediate Term

4. Emergency Planning - Onsite Planning, Offsite Emergency Planning Zones (EPZ), Graded Response
5. Containment Leak Rates - Leak Rate Testing, Undetected Breach of Containment Integrity
6. Control Room Habitability - Filtration and Leak-Tightness Requirements
7. Environmental Qualification of Equipment
8. Accident Indemnification - Potential Renewal of Price-Anderson
9. Safety Issue Evaluation - Prioritization of Issues Using New Source Terms

III. Long Term

10. Offsite Contamination and Recovery
11. Siting - Explicit Consideration of Severe Accidents in Siting
12. Accident Monitoring and Management - Onsite and Offsite Instrumentation

benefit from the new knowledge now available on source terms. Similarly, a subcommittee of the Atomic Industrial Forum (AIF) on behalf of the nuclear industry also reviewed the current requirements and arrived at similar conclusions. These areas are now being subdivided into a group on which work can start soon and a group that has to await further development. Progress made in developing source terms will be reviewed periodically and will be compared against the information needed to initiate changes in current rules and regulatory practices. Changes will be initiated as soon as available information warrants it.

Proposed Schedule

The policy statement specified not only the goals of the program, but also its schedule. It stated that guidance on the role of PRAs will be issued within 18 months following issuance of the policy statement and guidelines and criteria for the examination of individual plants will be prepared within two years. A proposed schedule, consistent with this guidance, is presented in Figure 3.

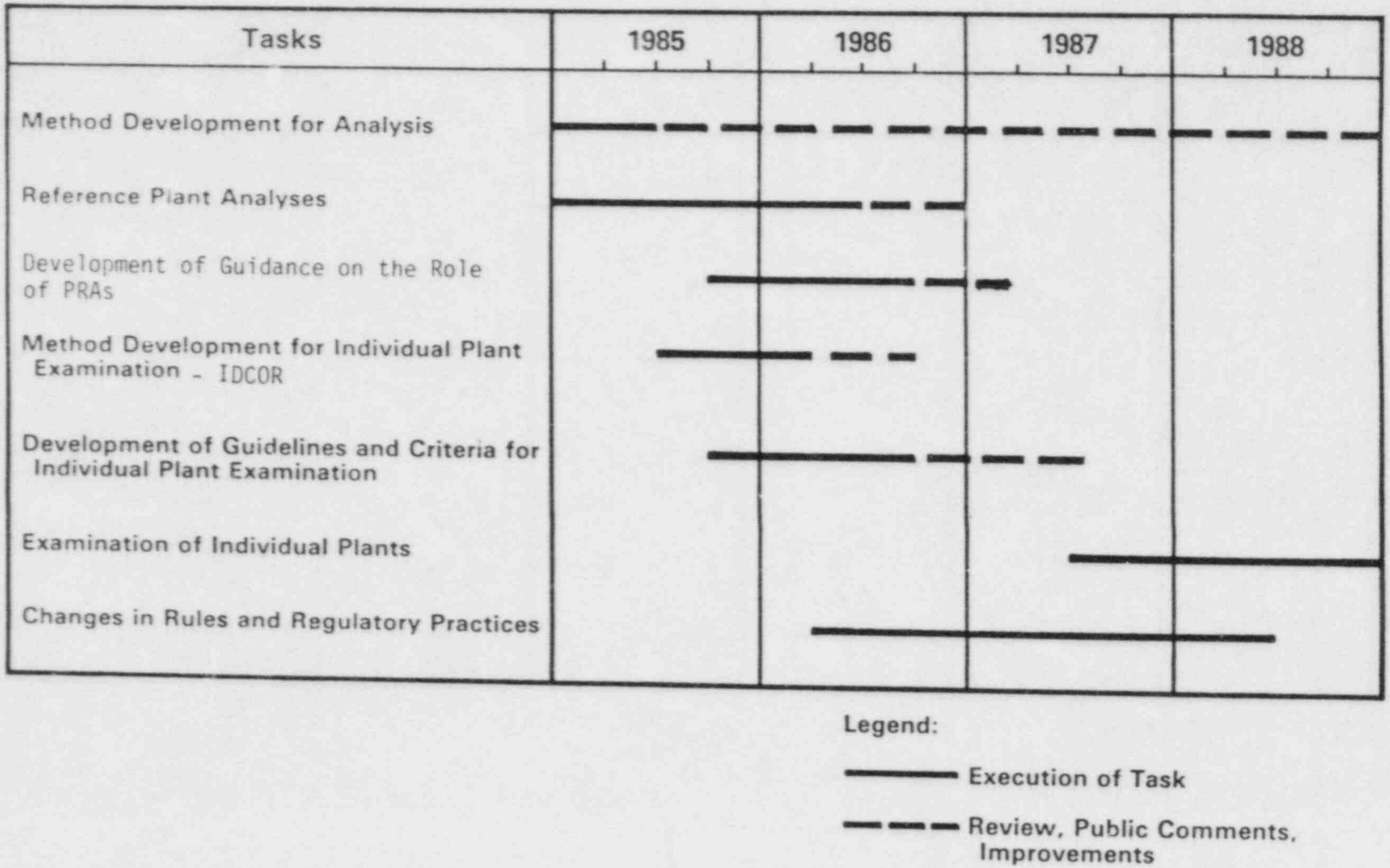
The first entry in the schedule, method development for analysis, indicates, that state of the art methodology available at mid 1985 is being used for plant analyses. However, the research program is continuing. New information available from the research program will be reviewed prior to each major milestone and the results or conclusions will be updated to reflect any significant, new finding. The reference plant analyses will be published for comment in mid 1986 and in final form by the end of 1986. The guidelines and criteria developed for individual plant examination is expected to receive the longest review period, including Commission consideration. Issuance of the guidelines is planned for August, 1987. Examination of individual plants will follow.

## The Role of PRAs in the Implementation Program

A combination of deterministic and probabilistic considerations will form the basis for severe accident decisions. Probabilistic analysis has the benefit of considering a broad spectrum of events, quantifying the results and combining them into a risk estimate. Use of PRAs will help to identify those contributors to severe accident risk that are clearly dominant and hence need to be examined for cost-effective risk reduction measures. It will also help to identify those accident sequences that are clearly insignificant risk contributors and can therefore be prudently dismissed. Furthermore, the quantification and combination of results will permit tradeoffs between accident prevention and mitigation and will provide means for the propagation of uncertainties through the analysis. The extent of the role PRAs will play in decision making will depend on the quality of the individual PRAs, on the magnitude of uncertainty associated with the PRA, and on the completeness of the probabilistic analysis.

FIGURE 3

# Proposed Implementation Schedule



It is recognized that there are a diversity of PRA methods. These will continue to undergo evolutionary development as the results of research programs and reliability data from operating reactors become available. Some of the PRAs used in the reference plant analyses are rather old (10 years old), others are more recent. Weaknesses identified in some previous PRA reviews include treatment of system interactions and common mode failures, and consideration of human involvement in the course of severe accidents. Since the main goal of the implementation program is identification of severe accident vulnerabilities the importance of system interactions and common mode failures as severe accident initiators as well as contributors to the progression of accidents should not be overlooked. Likewise, the effectiveness of human performance should be emphasized. Both, negative impact of human performance on severe accident risk and its potentially positive contribution to limiting the consequences of severe accidents should be considered. As part of the NRC review of the reference plant analyses, it is our intent to examine the quality of the probabilistic analyses used. For example, this could be done by selecting a few systems interaction events, common mode failures and human action, which either have been observed in operating plants or are judged to be important to the conclusions of the plant evaluations. Then, examine the treatment of these events in the reference plant analyses. The outcome of the examination will influence our reliance on the PRA in question.

The uncertainties of severe accident analysis are expected to be large and complex. Thus, consideration of uncertainties is a difficult and important task. As mentioned earlier, one of the benefits of probabilistic analysis is that the results are quantified and are combined together into a risk profile of the plant. Similarly, the uncertainties of the analysis need to be quantified and combined into an uncertainty estimate on risk. While it is recognized, that due to the lack of data and large scale experiments it is difficult to define severe accident uncertainties in precise mathematical terms, a decision still must be made on the extent uncertainties need to be covered in the plant examinations. Thus, a well defined goal on uncertainties should be established early in the implementation program and placed in front of the analysts evaluating uncertainties. Major contributors to the uncertainty of the results should be identified, and their effect should be traced through the analysis.

Finally, it is important that each step of the analysis be complete, cover all potential possibilities which could have a significant effect on the results, or on the uncertainties of the results. The fault tree analysis leading to accident likelihood should consider interaction among systems, commonality of components, and consequential failures in a realistic manner. Operating experience of nuclear plants seems to indicate that previous studies and risk assessments under-estimated these failure modes. Selection of severe accident sequences for analysis is an other important step. It is hard to predict ahead of time which sequences might be dominant with respect to risk. The main difficulty seems to be with the consideration of uncertainties. While some sensitivity studies on single parameters are available, the combined effects of a number of parameters on a given sequence are not well known. Understanding and modeling of physical phenomena in the analysis of dominant sequences also needs special attention. The ongoing research program has produced significant new developments on items like core melt progression, hydrogen release, fission product chemistry and containment performance. Old analyses need to be updated to reflect this new knowledge.

## Conclusion

The evaluation of existing plants and future applications with respect to severe accident vulnerabilities will be done using an approach that stresses deterministic engineering analysis and judgement complemented by probabilistic risk assessment. It is our intent to use information available from PRAs to the fullest possible extent, but with appropriate consideration of the specific PRA's quality and of the uncertainty associated with the analysis.

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# Risk Analysis of Decay Heat Removal Sequences During Shutdown

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## Summary

To help the nuclear utilities avoid accidents during shutdown and also to provide the technical basis for resolving a major generic safety issue, the NSAC Generic Safety Analysis Program has an effort in the area of shutdown decay heat removal (DHR). This paper describes the approach and conclusions of two risk assessments, NSAC-83, "Brunswick Decay Heat Removal Probabilistic Safety Study" and NSAC-84, "Zion Nuclear Plant Residual Heat Removal PRA." Generally risk during shutdown is less than at power. However, instantaneous risk can be greater depending on decay heat level and the maintenance and testing being performed. These analyses also indicate that cost effective safety additions can be achieved through minor design and operational improvements. Sensitivity studies did not show substantial risk reduction to be gained from added redundant DHR trains. DHR reliability has been modeled for potential accidents occurring during all modes of operation.

## The PWR Analysis

### Introduction

The PWR analysis was performed by Pickard, Lowe and Garrick under contract to EPRI. Commonwealth Edison and NSAC also participated in modeling the Zion plant during shutdown. Significant use was made of the "Zion Probabalistic Safety Study" (ZPSS) and NSAC-52, "Residual Heat Removal Experience Review and Safety Analysis-PWRs." The ZPSS and NSAC-84 together account for all accidents initiated at power or during shutdown. NSAC-84 does not cover external events which are covered in the ZPSS. Damage to fuel outside of the reactor vessel is also not in the model.

This analysis explicity addresses the risk at Zion during the entire time the plant maintains cold shutdown conditions. While considering the factors that tend to make shutdown conditions less demanding, it accounts for other factors that aggravate abnormal events during these periods. Much of the backup safety equipment is disabled for operational or maintenance reasons; the operators get little indication of some abnormal events; unusual plant lineups are commonplace during maintenance periods; solid plant operations can lead to rapid overpressurization events; events can progress quickly, if they occur before decay heat has fallen off and while coolant is still hot enough to support blowdown. Note that all the events analyzed in this study require operator intervention to prevent core damage; the plant does not protect itself automatically.

Results obtained for the Zion station have been examined to permit drawing inferences for other plants. The approach is to ask if Zion were to have been designed or operated in specific alternative ways, would the risk results have been different. While qualitative results of such considerations cannot be expected to apply directly to any other plants, operators of other units can get a feel for what might be significant risk factors for them by carefully comparing these discussions with their own facilities.

## Zion Specific Findings

- Risk, the combination of probability and consequences, is lower during most periods of shutdown than while at power.

- Operator actions are very important, contributing to all core damage scenarios; hardware failures (often human induced) are the dominant initiating events.

- There is greater uncertainty about the likelihood of operator errors than most hardware failures. If the median human error rates were more certain, the modeled risk during shutdown would be lower than calculated in this analysis.

- Loss of cooling events occur approximately once every 2 to 5 reactor years. LOCAs, rapid overpressure events, and losses of offsite power (LOOP) occur much less often.

- Loss of cooling events leading to loss of inventory by boil-off are the dominant contributors to core damage.

- The frequency of overpressure events in the range of 600 to 1,800 psia is high. It is dominated by spurious safety injection events after the safeguards actuation circuitry is reenergized during plant heatup.

- Early in an outage, the operators must respond quickly to LOCAs to prevent core damage: within about 30 minutes for the first 10 hours following shutdown and within 4 hours after 7 days. For some loss of cooling events, no alarms will actuate and the only control board indication before equipment damage is fluctuating RHR pump current due to cavitation.

- Overpressurization events with a solid plant generally are too fast for effective operator response. The automatic protection systems and mechanical relief valves are reliable. Pressurization between 600 and 1800 psia is not likely to cause vessel damage at Zion.

## Inferences about Risk

The following points can be inferred from the results of the analysis and sensitivity studies.

- The frequencies of severe LOCAs through the containment sump and spray valves would be higher at Zion except for the following factors:

  - Plant personnel do not manually operate motor-operated valves.

  - Operators are not permitted to override valve interlocks, even for tests. Rather, lineups are made to satisfy the interlock conditions prior to stroking the valves.

- Even if manually operated, sump valve interlocks will drive the valve shut when the hand clutch is released.

- Sump and spray valves are relatively inaccessible.

- Procedures and training to close the RHR suction valves in the event of a LOCA during RHRS for a plant not having a procedure during shutdown would:

  - Enhance the chance that LOCAs are terminated quickly.

  - Begin the required lineup for using low pressure safety injection.

  - Initiate a loss of cooling event, but if RHR suction valves were not closed, loss of cooling would begin anyway within about 15 minutes due to the LOCA.

  - Initiate an overpressurization scenario, but the RCS will be partially drained as a result of the LOCA; refilling and pressurization will be a slow process.

  - Significantly reduce the frequency of core damage for some plants with lower capacity charging pumps than Zion. (Some plants may not have enough charging capacity to make up boiloff when decay heat is still high.)

- Training and use of the Zion abnormal operating procedure for excess primary system leakage make it likely that Zion operators will shut the RHR suction valves in preparation for using safety injection if level is not stabilized quickly.

- The frequency of rapid overpressure events would be proportionally lower if the fraction of outage time in solid plant conditions were reduced.

- Some automatic warning of loss of level or RHR pump cavitation would ensure that operators are aware of abnormal conditions. With this warning, they would be more likely to prevent core and equipment damage.

- Supplemental relief capacity outside the system boundary of the RHRS at Zion (i.e., use of the letdown relief valve in the CVCS in addition to the PORVs) substantially reduces the frequency of overpressure events exceeding 1,800 psia.

## The Modeling Approach

Review of the industry RHRS experience in NSAC-52 shows a significant number of events that have threatened continued heat removal. These "initiating events" may be grouped into three categories.

- Loss of Coolant Inventory Events (LOCAs) in which the reactor coolant system (RCS) or RHRS boundaries are opened by mispositioning valves in a way to cause rapid loss of water.

- Loss of Cooling Events (LC) in which cooling is interrupted by loss of RHRS flow or loss of component cooling to the RHR heat exchangers.

• Cold Overpressure Events (COP) in which rising pressure challenges the reactor vessel overpressure protection system.

These events have occurred primarily because of human actions or design specific hardware interactions at each plant. Therefore, a generic data approach to the determination of initiating event frequencies for Zion was not the most appropriate methodology. Instead, a detailed process flow model was developed for cold shutdown and the approach to it (see Figure 1). In that model all key steps followed by the operators throughout each type of outage were considered. Initiating events occur at specific points along the process flow model as a result of procedural errors. Also initiating events as a result of human errors and equipment failures unrelated to the process flow steps were permitted to occur randomly over time.

For each type initiating event (LOCA, LC, and COP), accident scenario event trees were developed to model plant and operator response. Quantification of these models is dependent on plant conditions at the time of the initiating event. For example, if the plant is in a solid water condition at the beginning of an overpressurization event, much less time is available for the operators to respond than if there is a bubble in the pressurizer or it is drained. Sequences in the LOCA and LC event trees proceed to one of two end states, success or core damage defined as uncovering to the core mid-plane. The COP event tree is different, because the effects of cold overpressure are uncertain--high pressure could possibly lead to failure of the reactor coolant system boundary, including failure of the reactor vessel itself. Note that cold overpressure is not the same as pressurized thermal shock (PTS): for COP, no thermal gradiant, and hence, no thermal stress is present in the reactor vessel wall. Therefore, COP events are much less a concern than PTS transients.

Three COP end states are defined in this study:

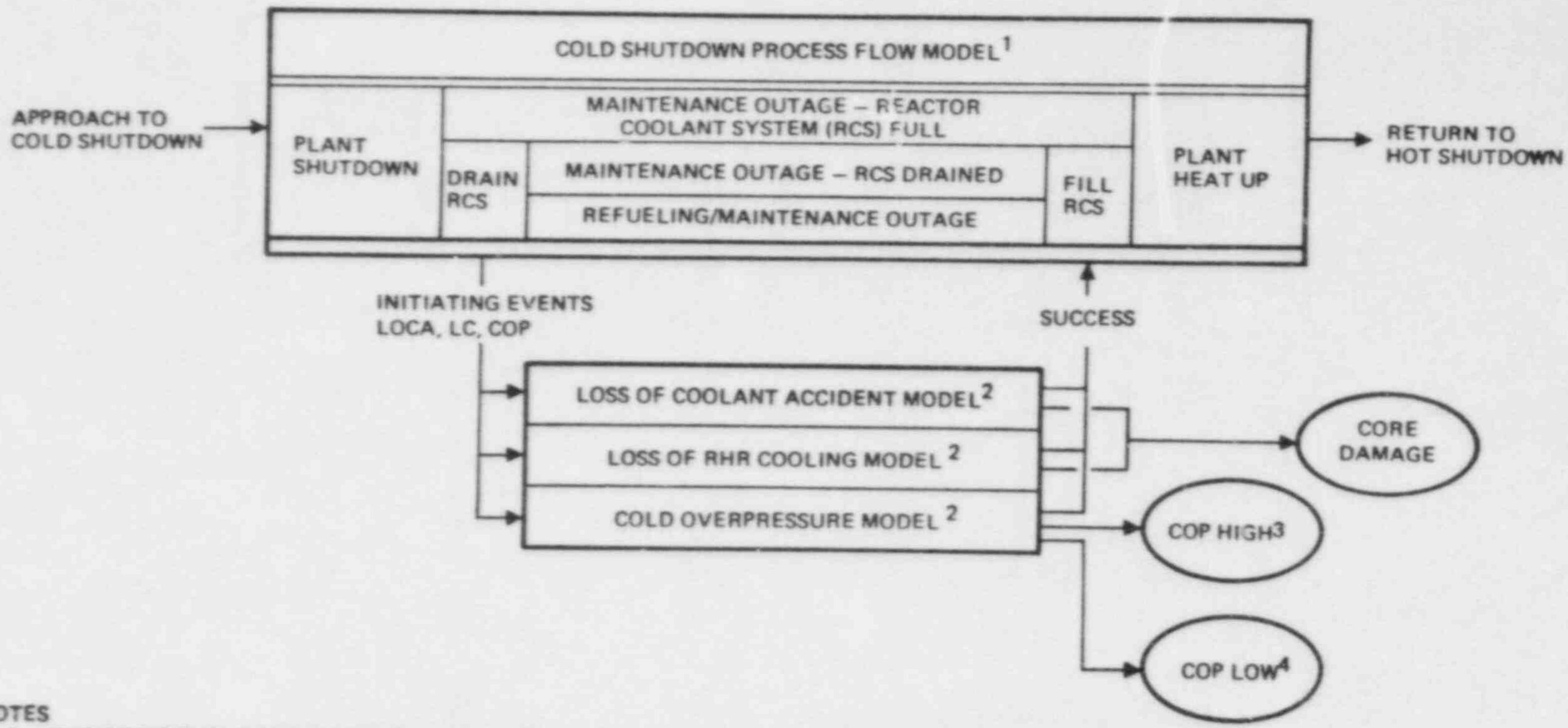
• COPLL - RCS pressure is maintained below 1,800 psia.

• COPL - RCS pressure peaks between 1,800 psia and 2,485 psia.

• COPH - RCS pressure exceeds 2,485 psia (setpoint for code safety relief valves).

It is not likely that any of these increasingly severe pressures will lead to core damage. The latter one will cause a LOCA somewhere in the reactor coolant system if pressure continues to rise. Unless the cold overpressure actually causes a catastrophic rupture of the reactor vessel, core damage is far from likely. Therefore, COPH, COPLL, and COPL are not added into the core damage category.

It is beyond the scope of this paper to model possible damage to the pressure vessel. However, the Zion Technical Specification limiting pressure at 250°F is 900 psia. The 900 psia limit is based on many conservatisms including a safety factor of two. In evaluating past operating experience, the NRC has not included this safety factor of two in analyzing vessels that have exceeded the normal operating P-T limit curves at low temperature. Therefore, the 1,800 psia limit for COPLL used in this study represents an assumed pressure




NOTES

1. INITIATING EVENTS FROM THE PROCESS FLOW MODEL OCCUR BOTH RANDOMLY OVER TIME AND AS A FUNCTION OF THE PROCESSES MODELED.

2. RESPONSE OF THE ACCIDENT MODELS IS A FUNCTION OF PLANT CONDITIONS AT THE TIME OF THE ACCIDENT, i.e., TIME OF THE ACCIDENT AFTER SHUTDOWN, DECAY HEAT LEVELS, RCS TEMPERATURE AND PRESSURE, STATUS OF PLANT EQUIPMENT.

3. PRESSURE EXCEEDS 2,485 PSIA FOR COP HIGH.

4. SEQUENCES LABELED COP LOW IN THE COLD OVERPRESSURE MODEL ACTUALLY GO TO TWO DISTINCT END STATES DEPENDING ON THE EXACT SCENARIO LEADING TO THE COP INITIATING EVENT IN THE COLD SHUTDOWN PROCESS FLOW MODEL:

   – COP LL, PRESSURE RISE IS STOPPED BETWEEN 600 PSIA AND 1,800 PSIA.
   – COP L, PRESSURE RISE IS STOPPED BETWEEN 1,800 PSIA AND 2,485 PSIA.

Figure 1. Overview of Cold Shutdown Modeling Approach

below which there would be minimal safety concern or regulatory imposed inspection requirements.

The Frequency of Core Damage

The definition of core damage in this study is the point when water level falls to the core midplane. This conservative choice was assumed with consideration given to the opinions of IDCOR experts. There is little chance of significant core damage until after the level falls below that point. However, the degree of conservatism is not relevant to this study. How likely it is that the operators will arrest the accident sequence in the interval between the water level reaching core midplane and actual damage has no impact on the conclusions. Substantial time has been modeled for operator response, and all cues to the operators have been considered.

Existing PRAs for power operations show that if releases are delayed until 12 to 24 hours after shutdown, the chances for observing early health effects are essentially eliminated. Furthermore, even if the containment is open, operation of sprays will greatly reduce all health effects. Finally, if the containment is closed, containment failure due to overpressure becomes less likely as core decay heat falls during an outage If the containment remains intact, health effects from core damage after a short time on RHR would be small.

The frequency of core damage due to events during cold shutdown is shown in Figure 2 as a probability of frequency plot. The results during power operations from ZPSS are shown for comparison. Point values for the two cases are

| | Mean | Median |
|---|---|---|
| Cold Shutdown | $1.8 \times 10^{-5}$/year | $2.6 \times 10^{-6}$/year |
| Power Operations | $6.7 \times 10^{-5}$/year | $5.0 \times 10^{-5}$/year |

Although the mean frequency of $1.8 \times 10^{-5}$/year or once in every 56,000 reactor years is 27% of the frequency from power operations, the median frequency is only 5%. This difference is due to the broad uncertainty in the results for the case of shutdown conditions.

The uncertainty in operator response contributes significantly to the uncertainty (in terms of the ratio of the 95th percentile to the median) in core damage frequency.* Although most initiating events are caused by hardware failure--an RHR suction valve tripping shut is the most frequently caused event--failure of the operators to carry out needed actions is included among the dominant failures in the accident sequence event trees. Some form of operator action is required to terminate all core damage event sequences. For example:

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*Note that more traditional measures of uncertainty based on differences--for example, the standard deviation--are low; i.e., although we are sure the frequency is not much higher than the mean value, it may approach zero on a linear scale.

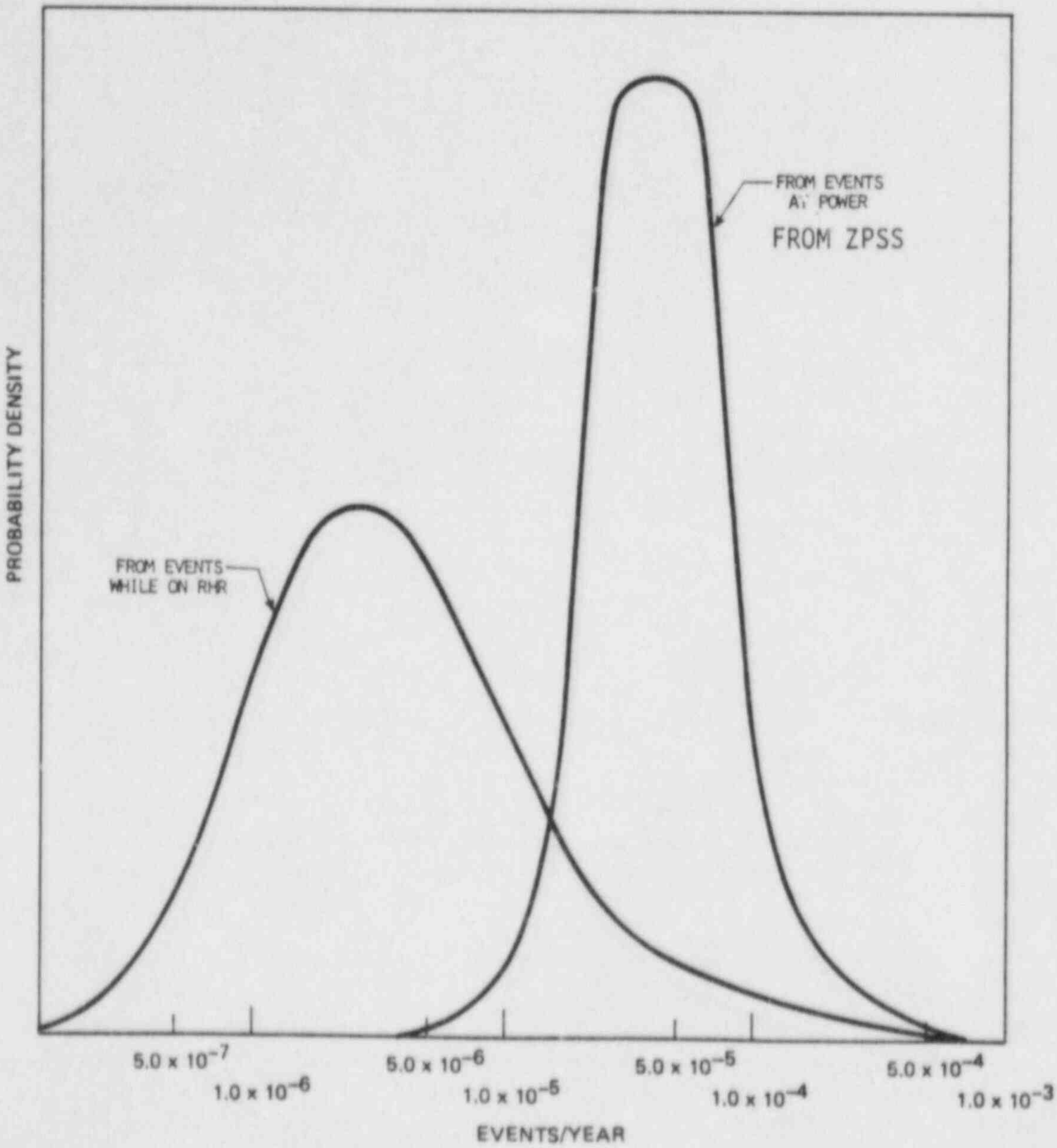


Figure 2. Probability Distribution for Core Damage

- Loss of cooling events require operator response to restore normal cooling. Except late in the outage when the charging flow is greater than the boil-off rate, some action is necessary within several hours to prevent core damage.

- LOCAs early in an outage can require response in as little as 30 minutes to prevent core damage. The most likely LOCAs during shutdown conditions with low RCS pressure require manual action to restore the RCS to its normal condition.

Not only are human actions required, but in some cases very few cues are available to the operators. Therefore, although there may be a good chance for operator success in most cases, there is a small chance that conditions will make operator failure likely. Such unfavorable conditions (few cues and short times to perform required actions) dominate the risk.

Modeling operator response is an imprecise art. Given the almost limitless factors affecting human performance, including external factors such as health, rest, and outside emotional stresses, it is all but impossible to define human action scenarios on a fine enough scale to eliminate major uncertainty. Thus, large uncertainties must be expected for a study of shutdown events requiring operator intervention.

Thirty-four sequences make up 90% of the total core damage frequency. The first 3 total 45%; sequences 4 through 15 range from 7% to 1% each, raising the total to 80%; and sequences 16 through 34, at less than 1% each, bring the total to 90%.

- Sequence 1 begins during a refueling outage. It represents a loss of RHR cooling during the maintenance period after refueling is complete (modeled as 1,660 hours).

- Sequence 2 begins during a nondrained (solid plant) maintenance outage. It represents a loss of RHR cooling during the modeled maintenance period of 358 hours.

- Sequence 3 begins during a drained maintenance outage. It represents a loss of RHR cooling during the modeled maintenance period of 982 hours.

In all three cases, the predominant cause of failure is that one of the RHR suction valves has tripped shut and the operator fails to determine if and what actions are necessary to restore cooling. Two causes for the trip are modeled. In the first, a maintenance action causes a suction valve to trip shut when a false high RHR pressure signal is generated by either a noise pulse from switching power supplies or by directly testing the pressure transmitter or interlock circuitry. In past years, these maintenance-caused trips occurred frequently. Because of changes to Zion administrative procedures requiring that the valves be deenergized during certain maintenance actions, spurious valve trips are now much less likely. The second major cause for loss of RHR cooling is random valve failure. Based on the Zion experience, this failure rate is higher than the generic, industry-wide value. Three valve trips of unidentified cause experienced at Zion are responsible for the higher rate. If these trips could be traced to maintenance actions that have been corrected rather than individual valve failures, the frequency of loss of cooling events and, hence, core damage would be lower by an approximate factor of 3.

All three scenarios branch to the loss of cooling event tree where they follow identical paths:

- "No Closure of RHR Suction Valves" is failed in the model since a valve tripped to cause the event.
- "Operator Trips RHR Pumps" fails.
- "Operator Determines Action to Restore Cooling is Required" also fails.

The problem is one of recognition in spite of the fact that there are a large number of possible cues calling operator attention to the loss of cooling event or the subsequent loss of inventory. Unfortunately, under some outage conditions, some of these cues are either nonexistent or less likely to be noticed because little occurs during such a shift to call operator attention to the instrument indications. The following cues to loss of RHR flow have been considered in this study:

- <u>Loss of RHR Flow</u>. Positive indication but no alarm.
- <u>RHR Pump Amps</u>. Oscillating indication due to cavitation, no alarm.
- <u>RHR Pump Trip</u>. Pumps may not trip even under extended cavitation.
- <u>Core Exit Thermocouples</u>. Alarm on plant computer, but many computer alarms exist during cold shutdown, and these are not likely to be noticed. Also, the plant computer may be shut down.
- <u>Pressurizer Level</u>. High then low level alarms only if pressurizer not drained.
- <u>Pressurizer Pressure</u>. Event begins below low pressure alarm setpoint; no high pressure alarm if the overpressure protection system (OPPS) works or system open.
- <u>Reactor Vessel Level</u>. Instrumented but no alarm.
- <u>If a Charging Pump is Operating</u>
  - Loss of inventory occurs more slowly, extending time available for detection of problem.
  - Volume control tank (VCT) low level alarm.

- VCT low level shifts charging pump suction to RWST, extending time for detection.

- If VCT low level shiftover fails, the charging pump will seize, trip, and alarm.

• If the RCS is Open.

- Steam may be visible; on the other hand, a slow steaming rate coupled with operable containment purge may make such an observation unlikely.

- Radiation monitors, especially stack monitors, should alarm, but if the RCS water is clean, this may not occur until after damage has begun.

• If the RCS is Closed.

- Zion's pressurizer relief tank (PRT) has high level and high temperature alarms; however, at many times during the outage the PRT high level alarm may already be activated; procedures require the PRT to be filled.

- Radiation monitors will behave the same as for open RCS.

• RHR High Temperature Alarm. Only works if the loss of cooling was due to loss of component cooling water (CCW) to the RHR heat exchanger. Other failure modes for RHR are much more likely than the loss of CCW.

To quantify an event where the operator fails to determine action is required, a three-dimensional matrix of conditions was constructed to arrive at the various combinations of conditions that define the event.

• Charging Pump Operating. Yes/no.

• Time of Event After Shutdown. 0 to 100 hours, 100 to 500 hours, and 500 to 2,000 hours.

• Time to Damage. For each combination of charging pump and time of event, time to damage was established.

For each unique set of conditions, the cues described above were reviewed for their effectiveness. Then, the probability of the operators on shift detecting the loss of cooling was evaluated. If not detected, the probability of a shift change occurring before damage was evaluated since the new crew should check water level and RHR flow before relieving the shift.

Other contributors are generally similar to the three discussed above, but much less frequent. Variations include scenarios which involve loss of cooling combined with maintenance and equipment failures preventing restoration of cooling. LOCAs do not begin to appear until sequences where they are linked to failures in the accident tree reminiscent of the loss of cooling cases already presented; i.e., no failure to recognize the condition. Finally, the loss of offsite power (LOOP) events are very modest contributors beginning with a sequence that contributes only 0.7% of the risk.

## The BWR Analysis

### Introduction

The analysis of decay heat removal sequences during shutdown is an important part of the study reported in NSAC-83. The Brunswick Decay Heat Removal Probabilistic Safety Study. This report, prepared by Impell Corporation, presents results of a safety study of the decay heat removal function at the Brunswick Steam Electric Plant (BSEP) which is owned and operated by the Carolina Power and Light Company (CP&L). BSEP is a two-unit boiling water reactor plant (BWR-4) with Mark I containments and steel lined concrete suppression pools and electrical capacity of 790 MW each. This study evaluates Unit 1, although historical reviews of decay heat removal operation at both units are included as part of the overall project effort.

The primary purposes of this study were to:

- Perform a reliability study of the Brunswick residual heat removal (RHR) system in its principal operating modes (i.e., suppression pool cooling and shutdown cooling modes of operation);
- Peform a probabilistic safety study of the residual heat removal function at BSEP for events initiated during power operation and for events during plant couldown and cold shutdown; and
- Perform sensitivity analyses of accident sequences to investigate uncertainty, possible station modifications, and generic applicability of results to other BWR plants.

The study is based on a quantitative probabilistic evaluation of the reliability of decay heat removal equipment under a variety of scenarios in which the plant's decay heat removal function is challenged. These challenges follow transients which result in a reactor scram, a planned shutdown or events which may occur while the plant is in cold shutdown.

The cold shutdown event sequences which are part of this study are initiated by the interruption of the decay heat removal function of the RHR system. Other cold shutdown event sequences, such as loss of inventory, are considered only if they result from a loss of residual heat removal.

### Analysis Methodology

The overall approach for analyzing the decay heat removal loss sequences is to (i) identify significant event sequences challenging the decay heat removal function, (ii) develop detailed fault trees for failure of the RHR system and backup systems to provide this function, (iii) quantify the fault trees with component failure data based on BSEP experience and industry generic data, (iv) apply initiator frequencies also based on BSEP experience and industry data, (v) apply recovery models to determine the probabilities of failure to restore faulted components and/or failure to implement alternative means of decay heat removal prior to reaching a specified plant endstate, and (vi) relate the resulting important sequences, in an approximate way, to endstates more commonly considered in PRA studies, e.g., containment failure and core damage.

A significant input throughout the course of the study is the draft NSAC review of residual heat removal experience at BWRs. The review summarizes actual RHR system losses, degradations and other less significant failures which have occurred over a period of time equivalent to approximately 130 reactor years of BWR operating experience. This experience is used to identify the types of failures which might be postulated at BSEP and to assist in the development of the analysis fault tree models. The experience review provides some quantitative input to the common mode failure modeling for RHR system components. It also provides a qualitative benchmark for evaluation of BSEP results, in terms of the relative frequencies with which the RHR system may fail in the suppression pool cooling and shutdown cooling modes, and the significant contributors to such failures.

Endstate Definition

Since all significant loss of decay heat removal sequences considered by this study eventually involve manual or automatic blowdown of steam from the reactor vessel to the suppression pool, the temperature of the pool and the associated containment pressure are important parameters. A suppression pool temperature limit of 200°F is the analysis endstate for this study to define failure of decay heat removal. The 200°F figure is a bulk pool temperature; portions of the pool may be boiling. Sequences that result in a loss of DHR prior to reaching the analysis endstate are defined as interruptions of decay heat removal. Allowable times for equipment recovery and restoration of the decay heat removal function are based on the time to reach this temperature limit. This endstate represents an undesirable plant condition, but it does not correspond to containment failure or core damage. Above this temperature there exists uncertainty regarding RHR pump and suppression pool capabilities; design conditions are exceeded and failure rates are unknown. Complex containment analysis beyond the scope of this study is required to assess conditions beyond 200°F.

While use of this endstate is appropriate to meet the objectives of this study, results are not directly comparable to other studies which address core damage. Therefore, an analysis was provided which extrapolates the analysis results to a core damage state by estimating the availability of numerous recovery options during the additonal hours to core damage.

There are numerous periods in plant shutdown which represents varying levels of decay heat generation, various plant activities, and differing risk from inventory loss as well as heatup of the suppression pool. Only results for failure of decay heat removal sequences during the first week of cold shutdown were considered quantitatively in this study.

Factors which influence the results for sequences initiated by loss of decay heat removal from cold shutdown and which reach the 200°F suppression pool endstate are listed below in a summary fashion.

| | |
|---|---|
| Failures of decay heat removal for events from cold shutdown ($year^{-1}$) | $6.9 \times 10^{-6}$ |
| Mission time (hrs) -- average length of a shutdown for which less than 24 hrs is available to recovery prior to the 200°F endstate | 62 |
| Recovery time (hrs) -- minimum time to achieve the 200°F endstate | 15 |
| Initiator frequency ($yr^{-1}$) | 8.3 |
| Probability main condenser cannot be restored in any mode | $6.8 \times 10^{-3}$ |
| Probability that RHR system fails in its fucntion of DHR | $1.4 \times 10^{-3}$ |
| Probability of non-recovery of the RHR system in its function of DHR | $8.7 \times 10^{-2}$ |

These results are presented in more detail in NSAC-83.

The report also includes the presentation of detailed cut-sets for cold shutdown. The contributions of individual component failures, human errors, maintenance unavailabilities, and system dependencies are evaluated. Their importance to RHR system reliability is discussed; and, after considering opportunities for restoring the RHR system or backup systems, their importance to the failure of DHR is considered. In addition to the loss of cooling function, opportunities for loss of coolant through decay heat removal systems are handled in the fault tree analysis.

With the aid of results from other studies, core damage frequency is estimated to be in the range of $3.5 \times 10^{-7}$/yr for events from cold shutdown.

Twenty sensitivity studies are performed to address (i) uncertainty in data or analysis assumptions and (ii) potential procedural or hardware changes to prevent or mitigate loss of DHR. These studies also address the significance of unique features of the BSEP plant. This latter information pertains to events from power as well as from cold shutdown, and is important when using this study for generic conclusions about other BWR plants. Since sensitivity studies identified a strong sensitivity to the assumed need for RHR room coolers, a time-dependent heat balance study of the RHR room was subsequently performed. Based upon reasonable environmentally-induced-failure criteria, it was concluded that room coolers were not necessary for cold shutdown.

Conclusions

Conclusions from the study results which relate to the subject of this paper are put into three categories and are summarized briefly below.

A. Plant specific conclusions relating to events from cold shutdown:

- For events initiating from a loss of RHR decay heat removal in cold shutdown, the annual average frequency of achieving the 200$^\circ$F suppression pool endstate or core damage is small both in absolute value and relative to sequence frequencies initiating at power. However, because of high decay heat levels the instantaneous risk early in a cold shutdown may exceed the instantaneous risk during power operation.

- Risk from a significant failure of RHR decay heat removal decreases as the outage continues; that is, as decay heat decreases. After one week of an outage, available time to recover decay heat removal prior to the 200$^\circ$F endstate exceeds 24 hours. Verification of this conclusion for unusual maintenance situations would require a more detailed analysis.

- Principal contributors to a total failure of decay heat removal in cold shutdown include

  - maintenance unavailabilities of RHR and RHRSW system equipment.

  - failures of the RHRSW pumps and failures of the RHR pumps.

  - common mode RHR heat exchanger failure (however, the existence of backup heat exchangers at Brunswick reduces the safety concern from this contributor).

- Principal contributors to an interruption of decay heat removal in the shutdown cooling mode of operation are failure of the common pump suction valves F008 and F009.

- Interruptions of decay heat removal during cold shutdown generally have high probabilities for recovery before the 200$^\circ$F endstate because of long times available for recovery and numerous available backup systems.

- Failures of decay heat removal that result in reaching the 200$^\circ$F endstate generally have high probabilities for recovery before core damage because of additional long times available for recovery and opportunity to restore the main condenser or to employ containment venting.

B. Plant specific conclusions that address potential changes to prevent or mitigate loss of DHR:

- Equipment unavailability due to maintenance is a key contributor to the failure of decay heat removal. Policies to reduce maintenance unavailabilities, particularly coincident maintenance of more than one decay heat removal path and prolonged maintenance early in a cold shutdown outage, would enhance the decay heat removal function.

- The service water system plays a significant role in the decay heat removal process. A written procedure for cross-tying the two BSEP units' service water systems would improve the reliability of this decay heat removal option. The possibility for common cause failure of both units' service water intakes warrants attention. Training and procedures to use temporary DHR methods to extend service water recovery time would improve the likelihood of recovery from these loss of service water events.

- One design or procedural change which would significantly enhance the decay hat removal function would be to make the fuel pool heat exchangers available to both loops of RHR rather than just one loop.

- The added time for recovery made possible by use of the condensate storage tank inventory to delay suppression pool heatup would justify its use in a failure of decay heat removal scenario. Development of procedures for this option would enhance the decay heat removal function.

- Sensitivity studies show the benefit of proceeding slowly to cold shutdown as opposed to entering cold shutdown quickly with very high decay heat levels. Plant policies should discourage rapid cooldowns to begin maintenance or to shorten outage time.

- Timely indication of the interruption of decay heat removal enhances the recovery probability. Although enhanced indication does not greatly impact the failure of decay heat removal frequency, it does reduce the frequency of challenging plant safety systems.

C. The following conclusions apply at Brunswick and are expected to apply generally to BWR plants. The many plant specific conclusions above are also more broadly applicable, but plant specific variations must be considered.

- Advantage can be taken of diverse backup systems, numerous inventory control systems, and long time available to recover from faults to achieve low risk from failure to DHR.

- Existing plants have significant diversity and depth of DHR capability. Given procedures and operating practices which fully utilize this capability, the value of add-on systems or complex hardware changes is diminished.

Modeling differences and different endstate definitions preclude meaningful comparison of the numerical results of the BWR and PWR studies. The probability distributions shown in Figure 2 were derived by carrying the confidence intervals through the quantification of the PWR model. A set of similar distributions for the BWR model would have the same characteristics as Figure 2. Several design and procedural changes have been initiated at both plants modeled. These changes are the result of observations of problems encountered during operation and have been influenced by the results of these analyses.

# RAMONA-3B APPLICATION TO

# BROWNS FERRY ATWS*

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## 1.0 INTRODUCTION

The Anticipated Transient Without Scram (ATWS) is known to be a dominant accident sequence for possible core melt in a Boiling Water Reactor (BWR). A recent Probabilistic Risk Assessment (PRA) analysis[1] for the Browns Ferry nuclear power plant indicates that ATWS is the second most dominant transient for core melt in BWR/4 with Mark I containment. The most dominant sequence being the failure of long term decay heat removal function of the Residual Heat Removal (RHR) system.

Of all the various ATWS scenarios, the Main Steam Isolation Valve (MSIV) closure ATWS sequence was chosen for present analysis because of its relatively high frequency of occurrence and its challenge to the residual heat removal system and containment integrity. Therefore, this transient has been, and continues to be, analyzed by various organizations using various computer codes. However, most of the prior efforts have been carried out using point-kinetics codes.

Early deterministic analyses revealed a large variation in predicted power levels during an ATWS with the water level lowered to the top of the active fuel (TAF), as required by the Emergency Procedure Guidelines (EPG).[2] RELAP5/MOD1.6 results[3,4] predicted power levels of ~8%, which compared well with General Electric's statement[2] and Oak Ridge National Laboratory's prediction[5] of ~9%. On the other hand, the Electric Power Research Institute using spatial kinetics codes predicted[6,7] power levels of 15-18%. Therefore, with so many different predicted power levels, RAMONA-3B with 3D neutronics was used by the SASA program to provide best estimate ATWS calculations[8] with plant specific neutronic macroscopic cross sections from a TVA nuclear power plant using the P8X8R fuel.

The objective of this paper is to discuss four MSIV closure ATWS calculations using the RAMONA-3B code. The paper is a summary of a report being prepared[8] for the USNRC Severe Accident Sequence Analysis (SASA) program which should be referred to for details.

*Work performed under the auspices of the U.S. Nuclear Regulatory Commission.

## 2.0 RESULTS

The four MSIV closure ATWS sequences will be discussed in this section. The scenario and conclusions are presented with each transient.

### 2.1 Transient 1

The transient scenario overview is presented in Table 1. Essentially, this transient models a total failure to SCRAM followed by a recirculation pump trip at high pressure after the MSIV closure. The safety injection water (HPCI and RCIC) is throttled such that the downcomer water level drops to and then remains at the top of active fuel (TAF). Depressurization of the reactor vessel is initiated when the pressure suppression pool (PSP) water temperature reaches the value imposed by the heat capacity temperature limit (HCTL) curve for the PSP. This calculation has been performed mainly to evaluate the water level control of the Emergency Procedure Guidelines (EPGs). However, no boron injection or manual control rod insertion was performed.

The predicted reactor power (Figure 1) attained a maximum of 265% in about three seconds after the closure of the MSIV and returned to ~30% with the water level near the normal operating point. (For better resolution, the initial power spike on the plot is not shown.) As the water level was lowered to TAF (Figure 2) and maintained there, the power decreased to ~20% of rated power. This quasi-steady power level persisted until the HCTL was reached, requiring the operator to depressurize the system (Figure 3). This action increases the negative worth of the void reactivity feedback (Figure 4) in the core because of the increase in the core average void fraction because of flashing in the vessel and the increase in the specific volume of the vapor. The overall effect was the reduction of the relative power to ~15% after the depressurization. (The reactivity plots of Figure 4 are used for qualitative analysis only.)

The integral effect of the predicted power history can be seen in Figure 5 where the PSP water temperature reaches the assumed HPCI failure point of 190°F in ~23 minutes. This may lead to the overpressurization failure of the primary containment in about 20 additional minutes[5] because of the large amount of water injected into the vessel (causing core power increase) after the LPCI and condensate booster pumps (CBP) become active by low system pressure.

### Transient 2

This transient has the same scenario as Transient 1 with the addition of the effect of manual rod insertion (MRI) superimposed on the calculation. Thus, in Table 2, the events are identical with the previous transient except that the MRI action begins after 150s.

The most difficult part of this transient was to determine a realistic control rod insertion strategy that an operator would choose along with a practical insertion speed. While it is true RAMONA-3B could easily be programmed to insert the high worth rods during the transient, the knowledge gained would be of minimal use since the code would insert rods based on data an operator does not have. There is no optimum insertion strategy because of the nature of the burnup process and the constantly changing control rod pattern during the life of a fuel cycle.

The information on the realistic insertion strategy was obtained from Mr. S.A. Hodge of Oak Ridge National Laboratory, who supplied Brookhaven National Laboratory with video tapes of ATWS simulation sessions conducted at Browns Ferry Simulator on August 20, 1983, under the auspices of USNRC/SASA program. The simulator session chosen to be used as the model for the RAMONA-3B calculation is shown in Figure 6. The resulting RAMONA-3B insertion pattern is shown in Figure 7 where it should be noted that the insertions after 1229s were assumed since the TVA simulator session ended by that time.

Additional information used in the transient was the fact that the rod worth has a maximum between notches 30 and 8 (i.e., 4.5 to 10 ft.). Thus any rod near the maximum worth range would be driven in by an operator before a rod that would be inserted from the withdrawn position. Also, the RAMONA-3B control rod insertion was ended at 10.4 ft. to simulate the operator's stoppage of the insertion process outside the maximum worth band of a control rod.

Another important piece of information taken from the video was the fact that the operator, during the ATWS, could not devote all of his time to the control rod insertion process. Therefore, in RAMONA-3B calculation, a speed of 2 in./s (out of a maximum of 3 in./s) was used to insert any given rod. As a final point, it should be noted that a 1/4 core model was used (the checked lines in Figures 6 and 7) in the RAMONA-3B calculation implying that the negative reactivity insertion history for the model is different from reality where only one rod is driven into the core at a time. However, after the four (or two for boundary) rods have been inserted, the correct rod worth is present.

The results of the MRI action can be seen in Figure 8 where the relative power is reduced to 14% at 389s with 6 rods inserted and the power drops to 11% by 1229s with 20 rods inserted. This should be compared against the previous calculation (i.e., Transient 1) where the average relative power over the simulation was ~18% (Figure 1). The depressurization began at 1229s when the HCTL (i.e., 160°F) for the system pressure was reached (which was the same PSP water temperature used before). The negative SCRAM (or control rod) reactivity shown in Figure 9 along with the increase in void reactivity (i.e., increase in core average void) caused by the depressurization is enough to put the reactor on a negative period resulting in a predicted relative power of ~5% at 1410s. However, power/void oscillations can occur since the positive reactivity from the Doppler (i.e., fuel temperature) and moderator temperature feedback is larger than the negative scram reactivity. The power would increase until a certain void fraction is reached (i.e., when the reactivities sum to zero and turn negative) causing the power to decrease. This will generate less voids resulting in an insertion of positive void reactivity - causing the cycle to repeat itself. Power spikes should be avoided because of the effects on the system and their effects on the operator's instrumentation. Of course, these power spikes will eventually be damped out as soon as the operator inserts enough rods to introduce sufficient negative SCRAM reactivity to nullify the positive reactivity. To determine how many rods are needed for the control rods to become the dominant controlling factor for these mitigative actions, another calculation was performed without depressurization.

In Figure 10 the MRI calculation without depressurization indicates that the relative power drops to about ~6% after 28 rods (at ~1700s) have been inserted. Figure 11 shows that by 1800s the SCRAM reactivity is clearly the dominant negative reactivity controlling the effort to bring the plant to a hot shutdown. The void reactivity (i.e., core average void) still determines the resultant power level, but its effect is greatly reduced. (An example of a power/void spike can be seen at ~1650s in Figure 10, and the corresponding void reactivity swing can be found in Figure 11 at the same time.) Consequently, if the operator depressurized the system after inserting ~32 rods, the core should attain the decay heat level with low amplitude power/void spikes until a sufficient number of rods have been inserted to completely remove the void effect (i.e., hot shutdown).

The heatup rate for the PSP for all three transients discussed so far can be seen in Figure 5. While Transient 1 went into HPCI failure at 23 minutes, both MRI predictions show a large delay in the time to HPCI failure. The MRI heatup rates would level off since both calculations were terminated at low power. The PSP water temperature could eventually be turned around if the residual heat removal coding system (i.e., the RHR with its ~3% of rated power cooling ability) could be turned on. Otherwise, HPCI failure is inevitable with all its repercussions.

As a final comment for Transient 2 (which applies to all ATWS best estimate calculations), strong spatial effects in neutronics were obvious throughout the calculation because of the strong void feedback. A good example of this effect can be seen in Figure 14 were the axial power shape at 1229s completely inverts as compared to the power shape at other times. This demonstrates that point kinetics cannot be used for ATWS calculations. These conclusions have recently been stated in a letter[10] discussing knowledge learned in using the TRAC-BF1 code at INEL with 1D neutronics. The statement in the letter reads as follows:

> "During the mitigation of an ATWS in a BWR, the operator can implement a reactor power reduction technique termed level control. Level control is implemented subsequent to the tripping of the recirculation pumps, and introduces into the reactor an independent degree of freedom which is normally controlled by the automatic level control system. By dropping and maintaining the liquid level in the downcomer at a level equivalent to the top of the active fuel, the overall power of the reactor can be reduced. Such a dramatic change in the liquid level in the reactor has equivalent effects on the void, flow, and power patterns within the core. It has been observed that during the mitigation of hypothetical ATWS, large variations in these patterns can take place. The large

changes in, for example, core void profile, which level control necessitates results in large variations on the neutron flux and reactor power profiles within the core. These variations couple back to the thermal-hydraulics in a manner which cannot be separated. Thus, it becomes very difficult, if not impossible, to apply a point kinetics model to the simulation of such a transient."

Transient 3

The scenario for this transient can be found in Table 3. Essentially, the events are similar to the previous scenarios except for the fact that the HPCI system fails to operate. However, before discussing the RAMONA-3B predicted results, a brief description of the 1D neutronic core model used for this calculation should be given.

The 1D set of macroscopic cross sections used for this transient was generated from the 3D set of cross sections used for other transients. By using the RAMONA-3B method[8] to collapse the 3D cross sections down to an equivalent set of 1D cross sections, an accurate and fast running plant model was created to run RAMONA-3B in the 1D mode. An example of the success of this effort is shown in Figure 12 where the power histories of the two calculations almost coincide for 800s. This in itself is extremely convincing that the 1D cross sections are accurate. In addition, comparison of the total reactivities, Figure 13, proves that both sets of cross sections produce similar core reactivities responses for the same stimuli. This not only validates the RAMONA-3B 3D to 1D collapsing method, but also verifies the 1D coding in RAMONA-3B. The 1D version ran at a CPU-TO-REAL Time ratio of about 2.

The downcomer water level history is presented in Figure 15. After 70s the water level drops below TAF since the RCIC and control rod drive hydraulic system (CRDHS) flow is not sufficient to maintain the vessel inventory. The effect of lowering the downcomer water level was a reduction in the hydrostatic driving head causing low flow through the core, as seen in Figure 16. The overall result was that a relative power level of 4% of rated power was predicted by RAMONA-3B at 150s, as shown in Figure 17. No CHF was detected during the simulation.

Transient 4

The scenario for Transient 4 is identical to that found in Table 1 except that the recirculation pumps do not trip off, i.e., they continue to run during the transient. Although it is recognized that this event is highly unlikely to occur, the simulation was performed to determine the reactor power, system pressure, and water level drop during such an event to study the importance of this trip.

The results are shown in Figure 18 through Figure 20. In Figure 19, the power is shown to stabilize at about ~80% of rated, making the steam flow below the rated maximum of the SRVs, which is 85% of full power steam flow rate. The system pressure, Figure 19, peaks at 1340 psia and levels off to ~1310 psia, dropping below the fracture pressure for the vessel. The most significant graph is shown in Figure 20 where the downcomer water level is shown dropping quickly because the mass of steam leaving through the SRVs is larger than the ECC water entering the reactor vessel. Essentially, this calculation dictates that the operator must verify that the recirculation pumps have tripped after an ATWS has been identified to be in progress, and this must be done quickly.

## 3.0 CONCLUSIONS

RAMONA-3B has been used to calculate four MSIV closure ATWS scenarios. Conclusions resulting from the study are summarized in this section.

3.1 Level control (Contingency #7 of EPGs) and pressure control reduce the reactor power from 30 to ~18% during the course of the transient. HPCI failure occurs in ~23 minutes. No MRI or SLC was modeled during the transient.

3.2 Level and pressure control along with MRI will delay the time to HPCI failure. Although the reactor power can be reduced to 6% with ~20 rods inserted after the system has been depressurized, power spikes are expected because the void reactivity is comparable to the SCRAM (or control rod) reactivity and both are below the positive reactivity supplied from the Doppler and Moderator feedback. By waiting until ~32 rods have been inserted before depressurization, the negative SCRAM reactivity would be the dominant feedback effect and large enough to lower power close to the decay heat level. However, the PSP water temperature is very high and HPCI failure will eventually occur unless the RHR cooling is turned on. No SLC was modeled during this transient.

3.3 The High Pressure Boil Off calculation (i.e., Transient 3) predicted that the power would drop to ~4% at 150s with the water level ~4ft below TAF. No CHF was detected during the first 150s. RCIC and CRDHS flow is enough to sustain ~2.7% of rated power without loss of liquid inventory in the vessel. Thus, if core power can be sustained below 2.7%, no fuel damage would be expected.

3.4 If the recirculation pumps do not trip during an MSIV closure ATWS, the core power stabilizes around 80% of rated. Thus, the steam flow rate is below the SRV maximum capacity of 85% of full power steam flow rate. The peak pressure of 1340 psia was calculated. While the calculation showed that the reactor vessel was safe from an overpressurization failure, the water level was dropping rapidly. This calculation demonstrates that the operator should verify the recirculation pump trip as his first action during an ATWS event.

3.5 The RAMONA-3B calculations showed very strong neutronic spatial effects during the transients because of the void feedback in a BWR. Thus, point kinetics may not be appropriate for these transients.

## 4.0 RECOMMENDATIONS FOR FUTURE WORK

The boron injection (or SLC) issue was not addressed during these calculations. To evaluate the effectiveness of the SLC system, the problem of boron stratification in the lower plenum must be studied. While applying level control to an ATWS will reduce the power, it will also lower the flow rate into the core creating a reduction in the boron mixing efficiency. The effectiveness of the SLC system in mitigating an ATWS is a technical issue which remains to be solved.

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2. BWR Emergency Procedure Guidelines, Revision 2 (Draft) Appendix B, July, 1982.

3. W.C. Jouse, "INEL BWR Severe Accident ATWS Study," Eleventh Water Reactor Safety Research Information Meeting, Gaithersburg, Maryland, October 24-28, 1983.

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5. R.M. Harrington and S.A. Hodge, ATWS at Browns Ferry Unit One, NUREG/CR-3470, ORNL/TM-3902, July, 1984.

6. B. Chexal et al., Reducing BWR Power by Water Level Control During an ATWS: A Quasi-Static Analysis, NSAC-69, May, 1984.

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8. P. Saha et al., "RAMONA-3B Calculations for Browns Ferry ATWS Study," to be published November, 1985, Brookhaven National Laboratory.

9. G.C. Slovik et al., "RAMONA-3B Applications to Browns Ferry ATWS, Twelfth Water Reactor Safety Research Information Meeting, Gaithersburg, Maryland, October 22-26, 1983, NUREG/CP-0058 (Vol. 3).

10. Letter, Mr. T.R. Charlton (Manager, Reactor Simulation and Analysis program) to Mr. F.L. Simo (Director, Reactor Research and Technology Division), dated September 10, 1985. Subject: Transmittal Documenting the Reduction of Neutronic Cross Section Data for TRAC-BF1-TRC-120-85. (See Attachment by Mr. Wayne C. Jouse dated September, 1985, bottom of page 2.)

Table 1. Sequence of Events for Transient 1.

- MSIV closure in 5 sec.
- Failure to SCRAM.
- Feedwater flow ceases in 8 sec.
- Recirculation pump trip at high pressure.
- Downcomer water level hits lo-lo level and HPCI and RCIC ramp up to 5600 gpm in 25 sec.

At 150 sec. operator takes control.

- Lowers HPCI and RCIC flow to drop the downcomer water level to top-of-active (TAF), and maintains there.
- Operator follows depressurization line according to PSP heat capacity temperature limit curve.
- HPCI shifts suction from CST to PSP at high PSP water level (high level cf 15.2 ft).

Table 2. Sequence of Events for Transient 2.

- MSIV closure in 5 sec.
- Failure to SCRAM.
- Feedwater flow ceases in 8 sec.
- Recirculation pump trip at high pressure.
- Downcomer water level hits lo-lo level and HPCI and RCIC ramp up to 5600 gpm in 25 sec.

At 150 sec. operator takes control.

- Operator starts manually inserting control rods one by one.
- Lowers HPCI and RCIC flow to drop the downcomer water level to top-of-active fuel (TAF), and maintains there.
- Operator follows depressurization line according to PSP heat capacity temperature limit curve.
- HPCI shifts suction from CST to PSP at high PSP water level (high level of 15.2 ft).

Table 3. Sequence of Events for Transient 3.

- MSIV closure in 5 sec.
- Failure to SCRAM.
- Feedwater flow ceases in 8 sec.
- Recirculation pumps fail to trip at high pressure.
- HPCI and RCIC injections start when downcomer water level hits lo-lo level.

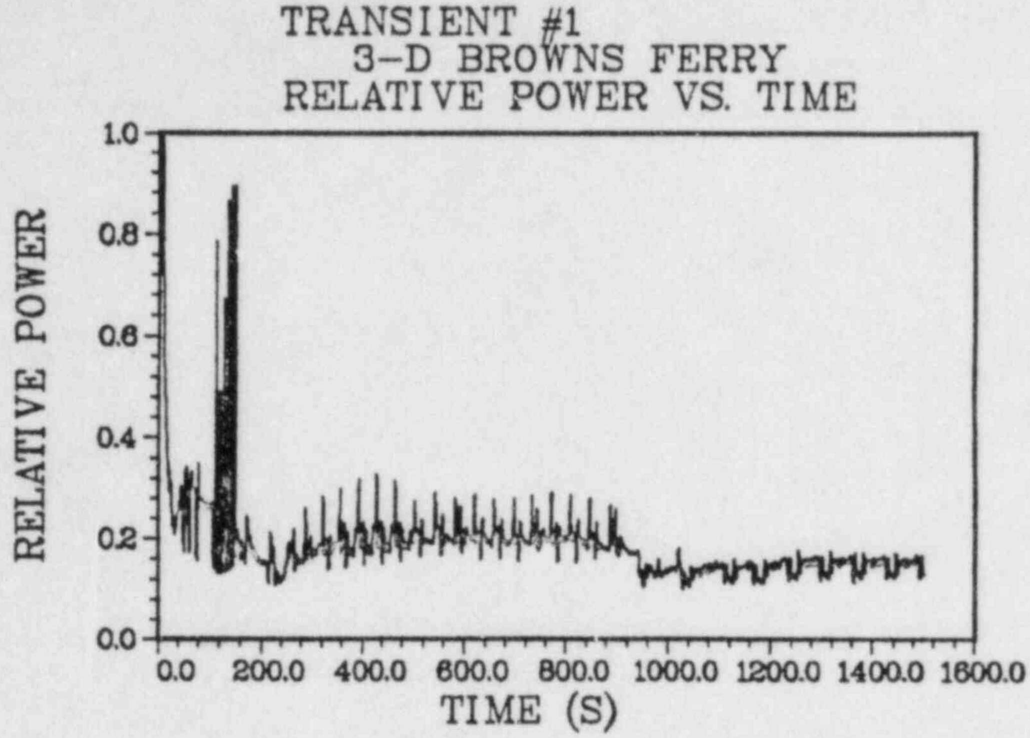



Figure 1. Reactor power prediction for Transient 1.

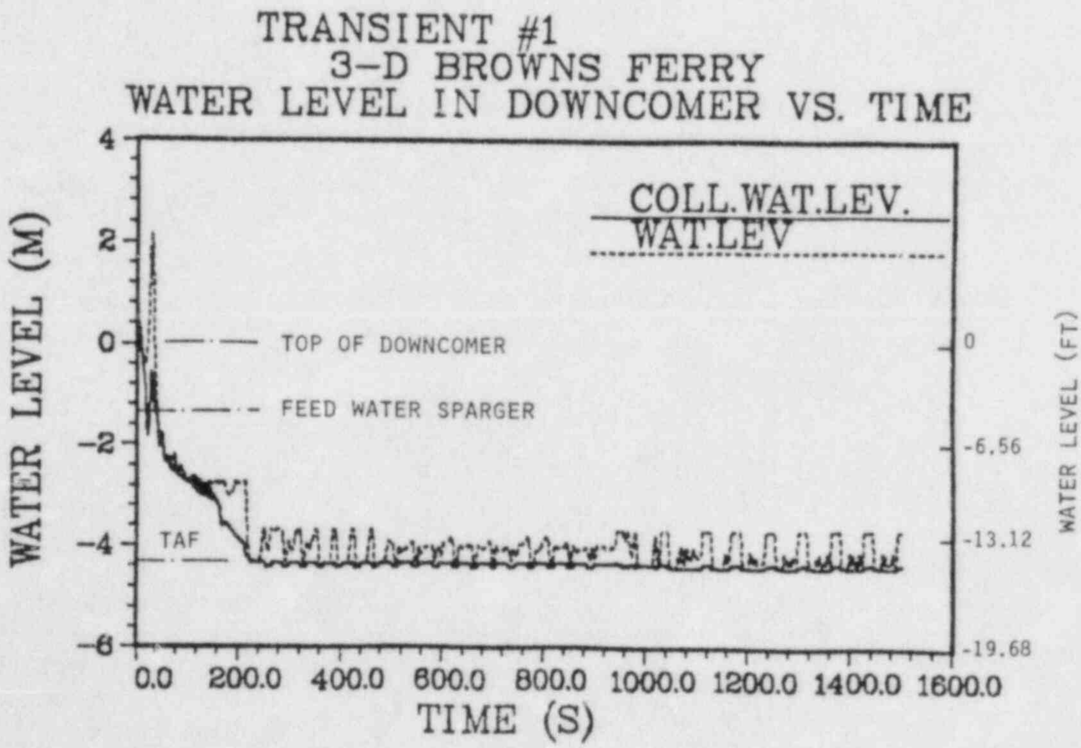



Figure 2. Downcomer water level prediction for Transient 1.

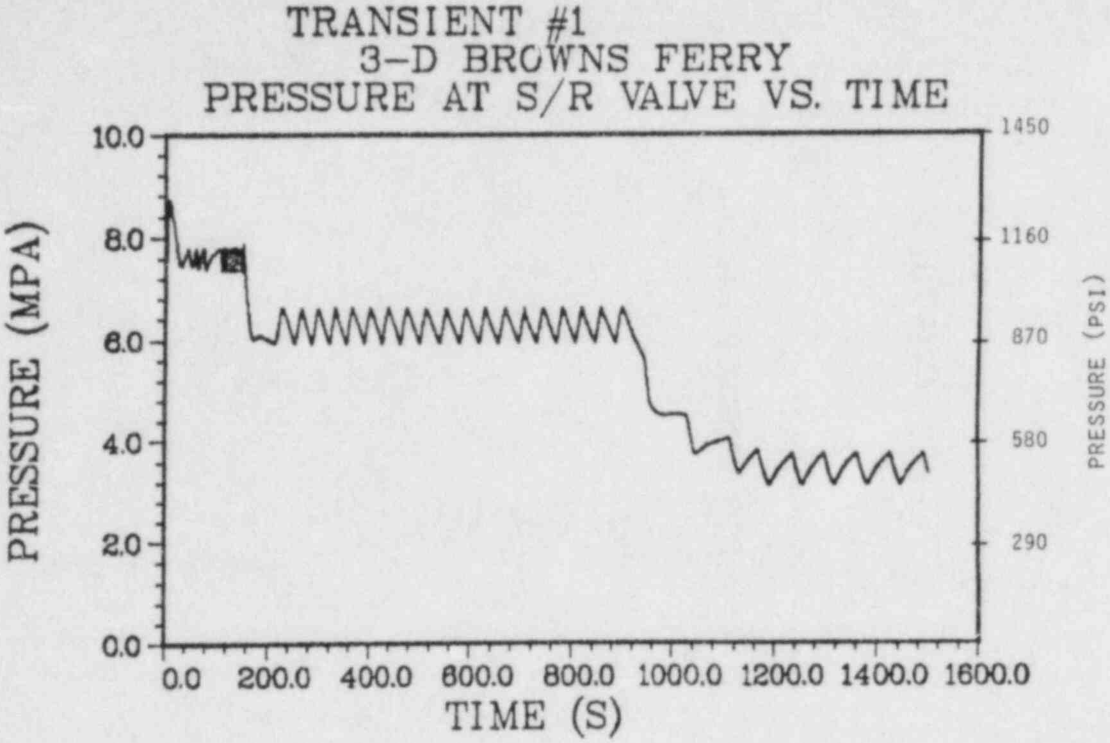



Figure 3. System pressure for Transient 1.

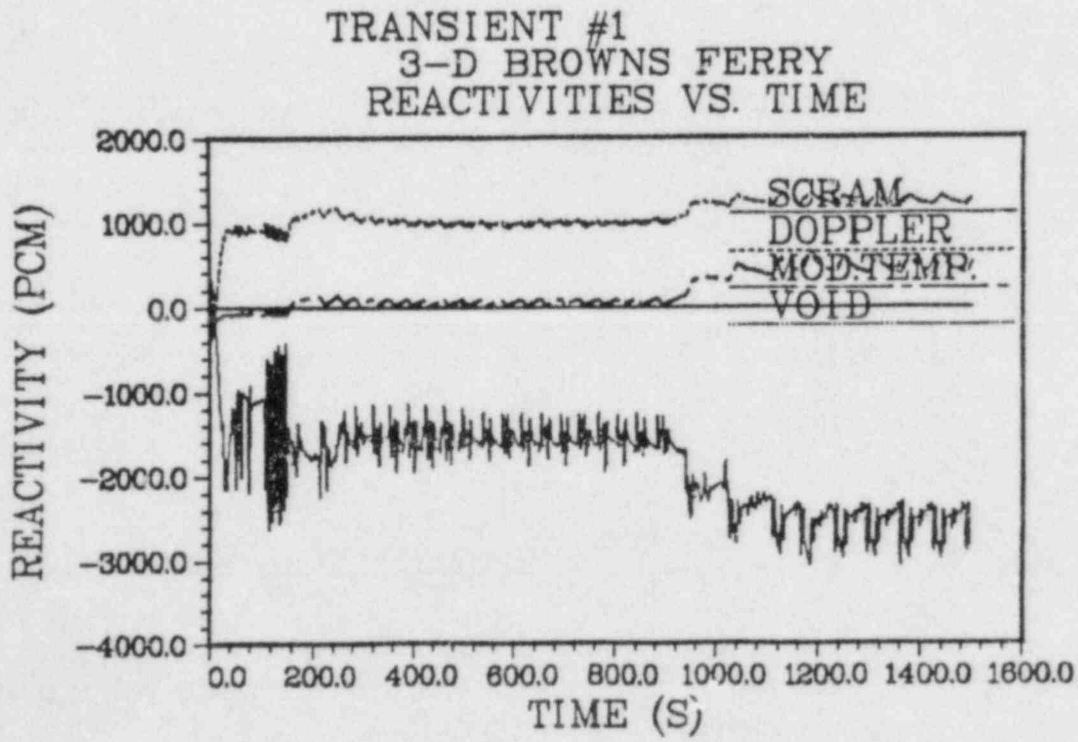



Figure 4. Reactivity predictions for Transient 1.

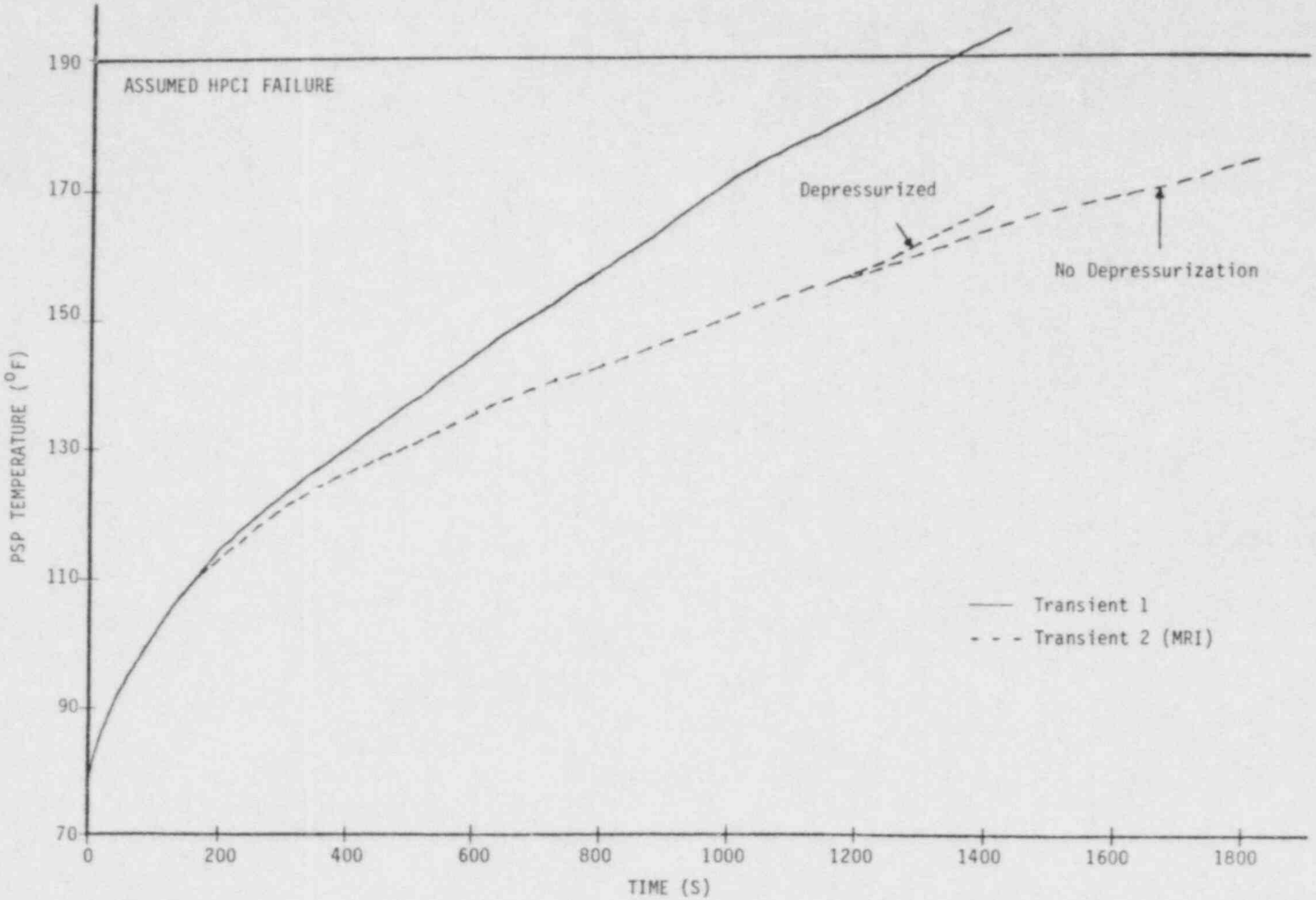
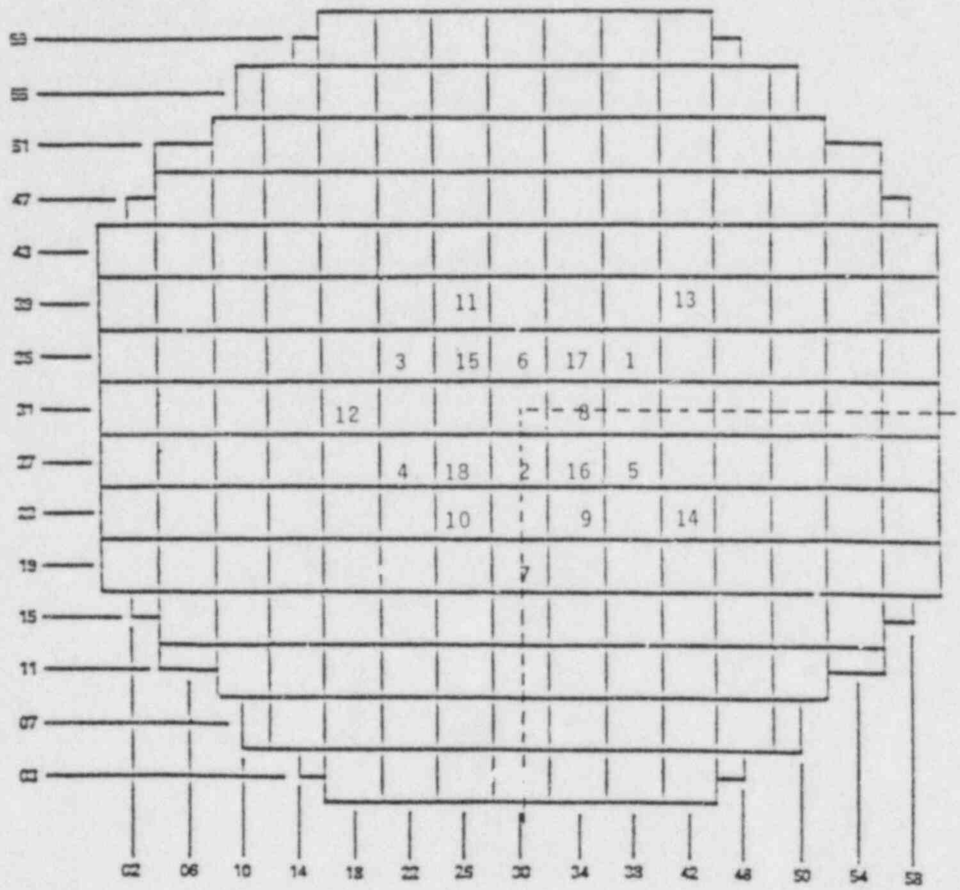



Figure 5. Comparison of PSP Water Temperature for Transient 1 and the Manual Rod Insertion Calculations

ROD INSERTION PATTERN AT SIMULATOR

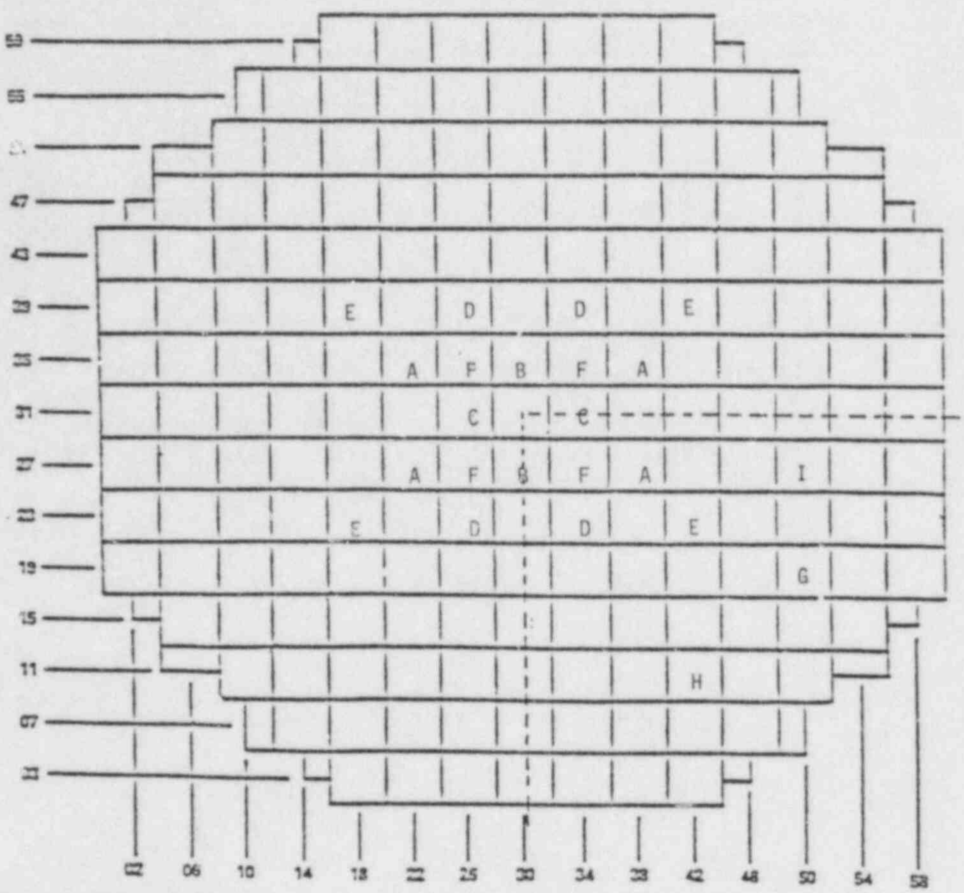
(TRANSIENT #7, AUGUST 20, 1983




* NUMBER INDICATES ORDER OF INSERTION

Figure 6. Control rod insertion pattern performed at TVA simulator during Session # 7.

RAMONA-3B ROD INSERTION PATTERN




| ROD SEQUENCE | TIME | MOVEMENT |
|---|---|---|
| A | 150 to 269 s | 5.3 to 10.4 ft |
| B | 269 to 389 s | 0 to 10.4 ft |
| C | 389 to 509 s | " |
| D | 509 to 749 s | " |
| E | 749 to 989 s | " |
| F | 989 to 1229 s | " |
| G | 1229 to 1469 s | " |
| H | 1469 to 1709 s | " |
| I | 1709 to 1949 s | " |

Figure 7. Control rod insertion pattern used by RAMONA-3B.

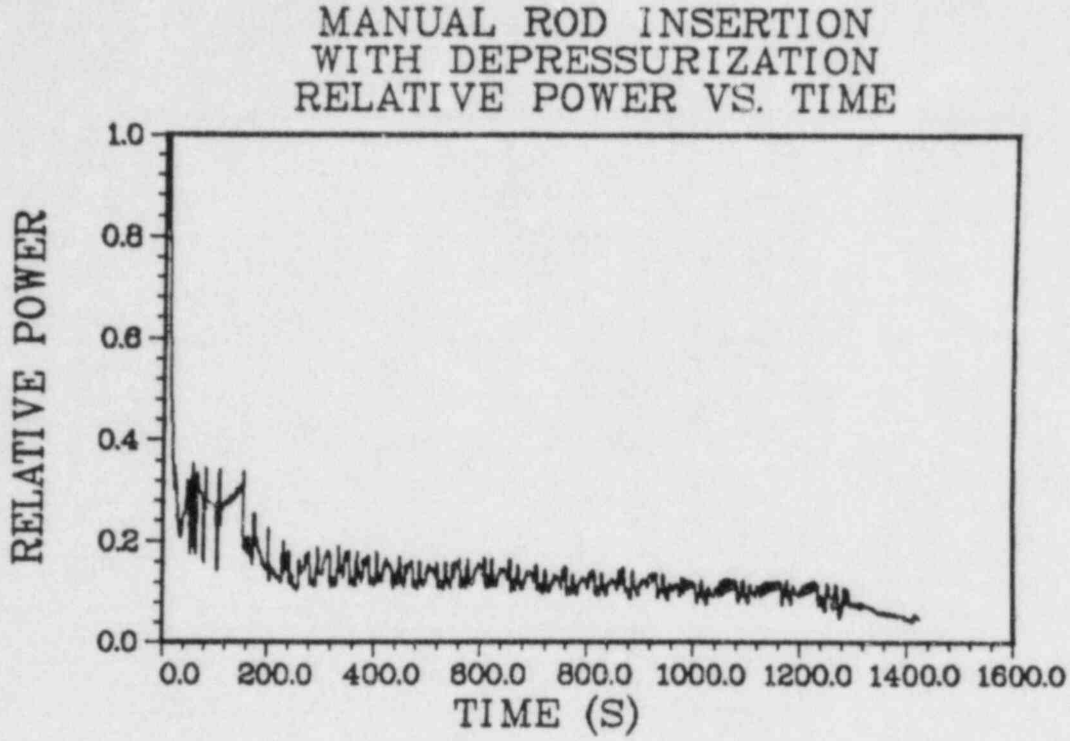



Figure 8. Reactor power history for Transient 2.

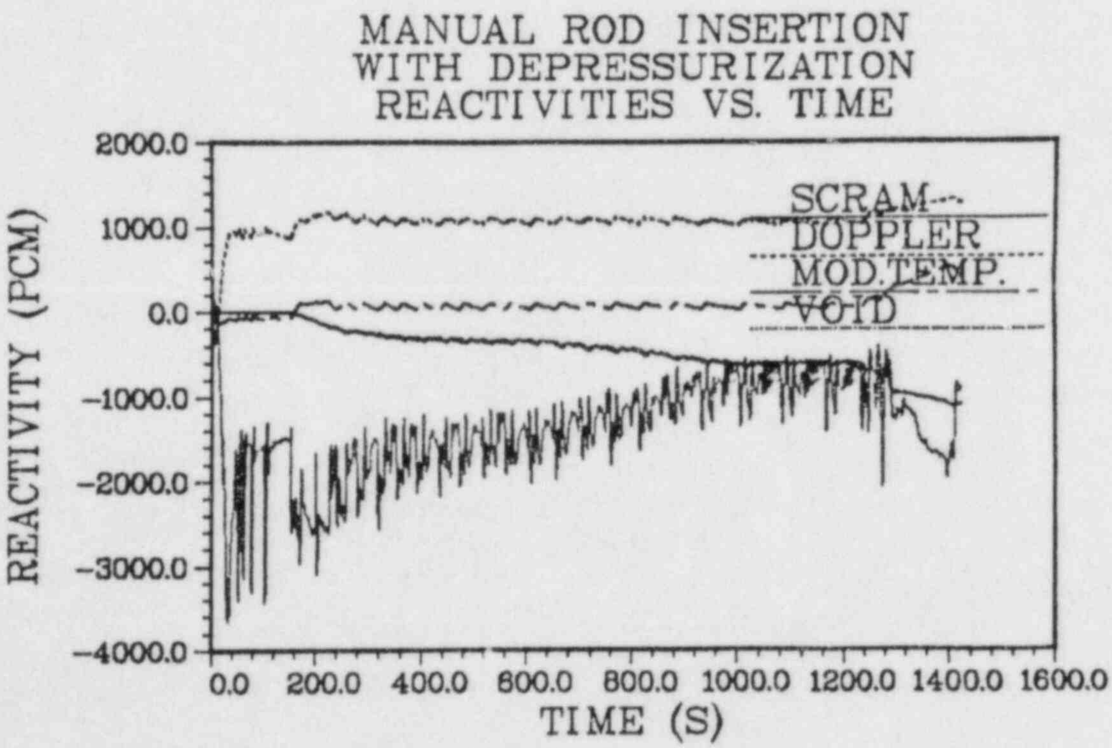



Figure 9. Reactivities for Transient 2.

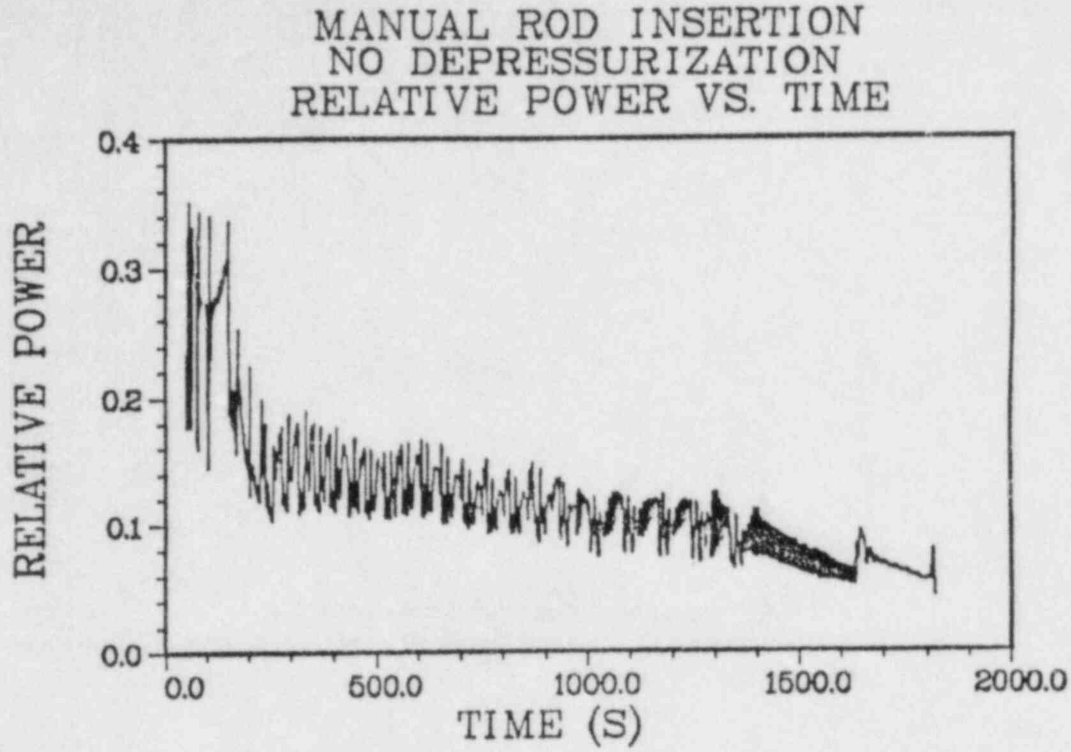



Figure 10. Relative power prediction for Transient 2 with no depressurization.

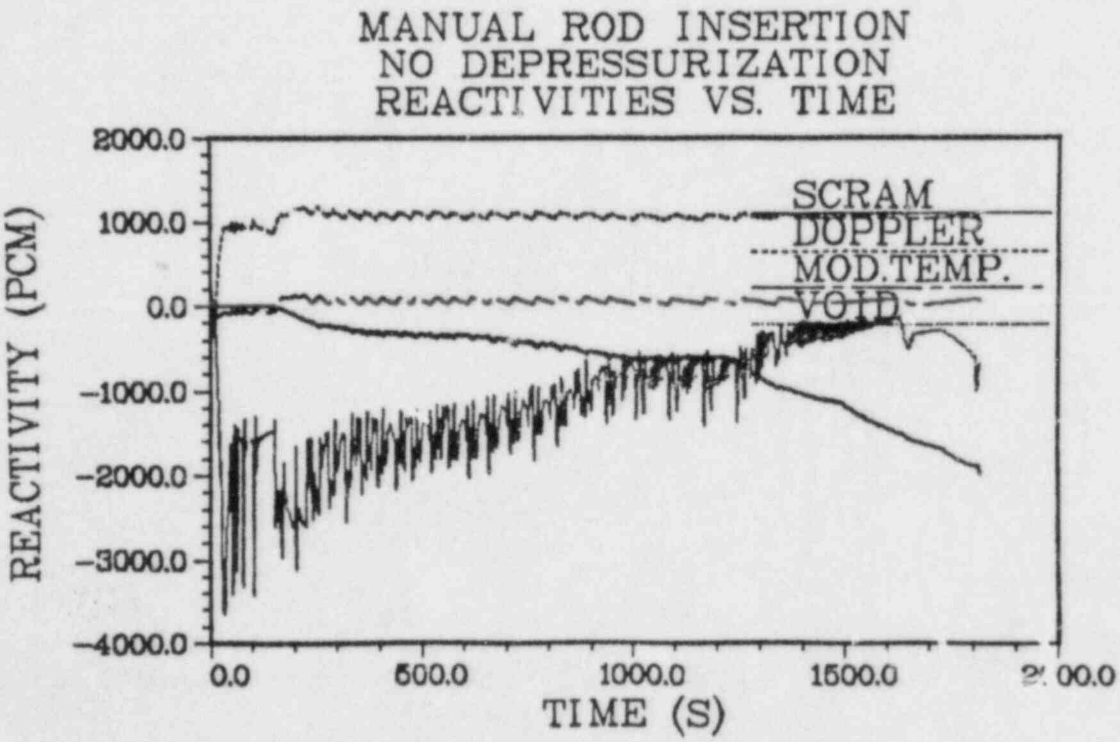



Figure 11. Reactivities for Transient 2 with no depressurization.

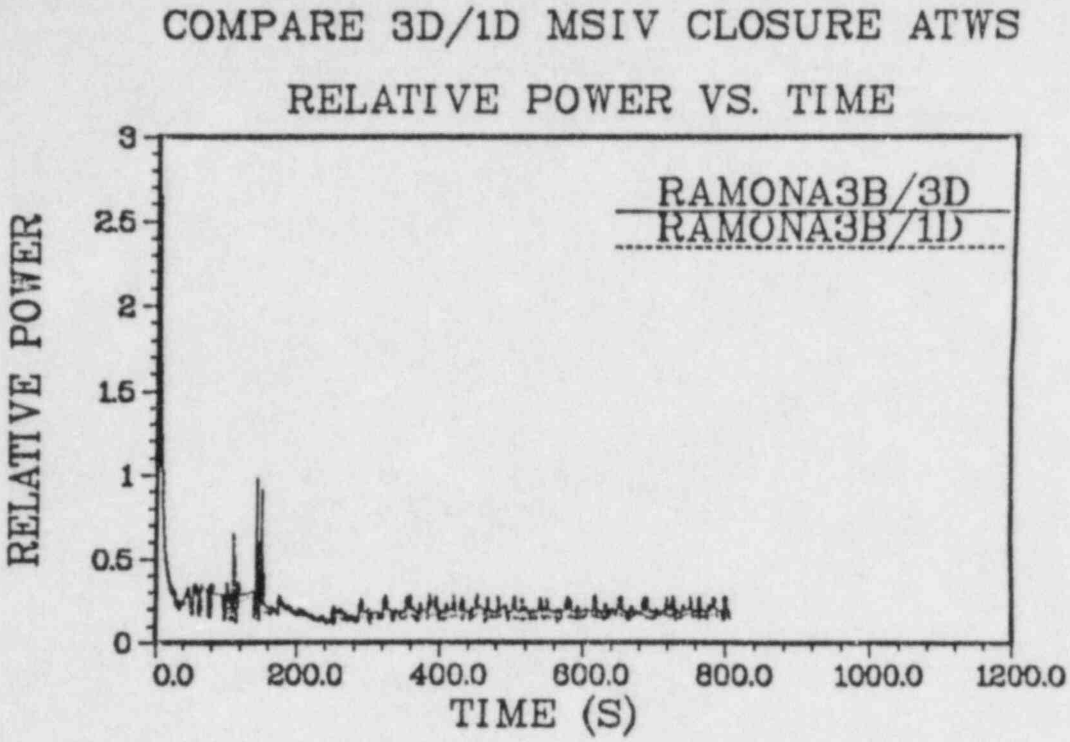



Figure 12. Comparison of reactor power for RAMONA-3B/3D and RAMONA-3B/1D.

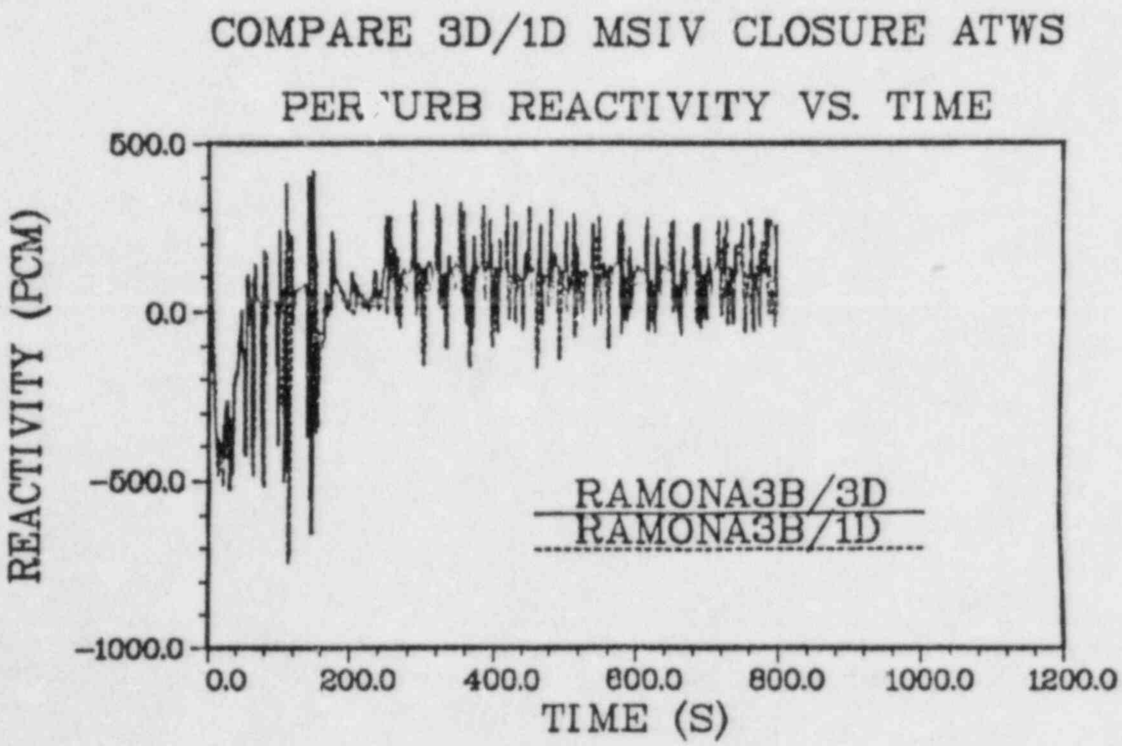



Figure 13. Comparison of total reactivities predicted for RAMONA-3B/3D and RAMONA-3B/1D.

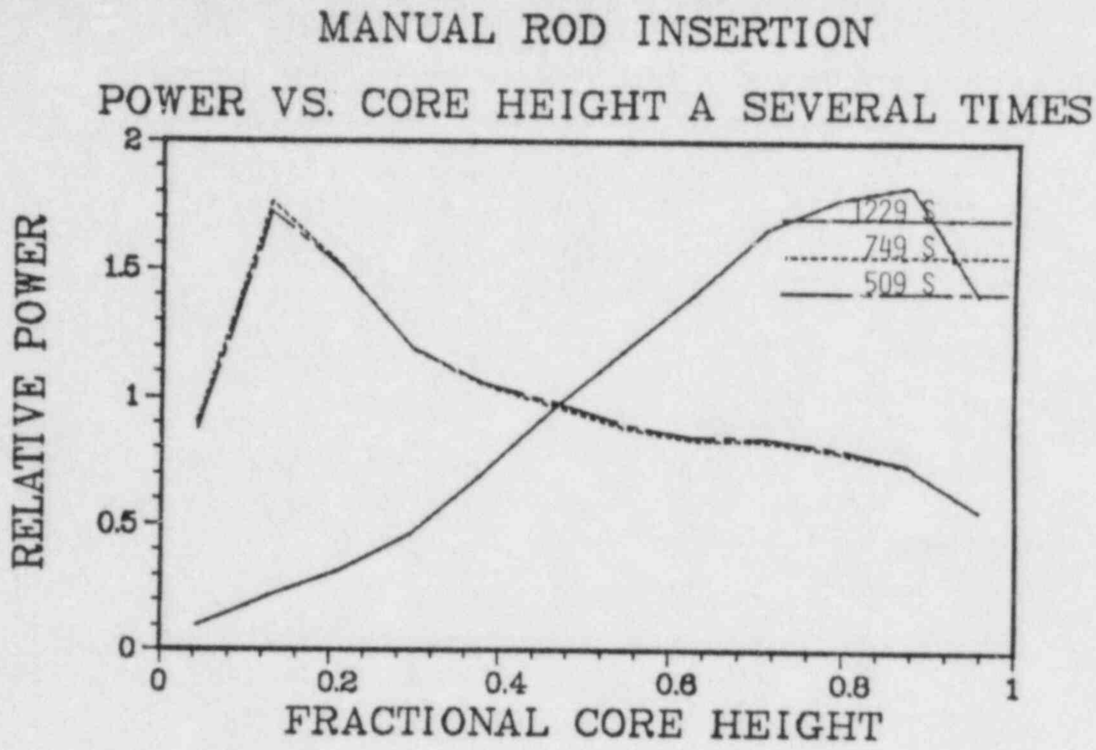



Figure 14. Axial power distributions for several times during Transient 2 (with depressurization).

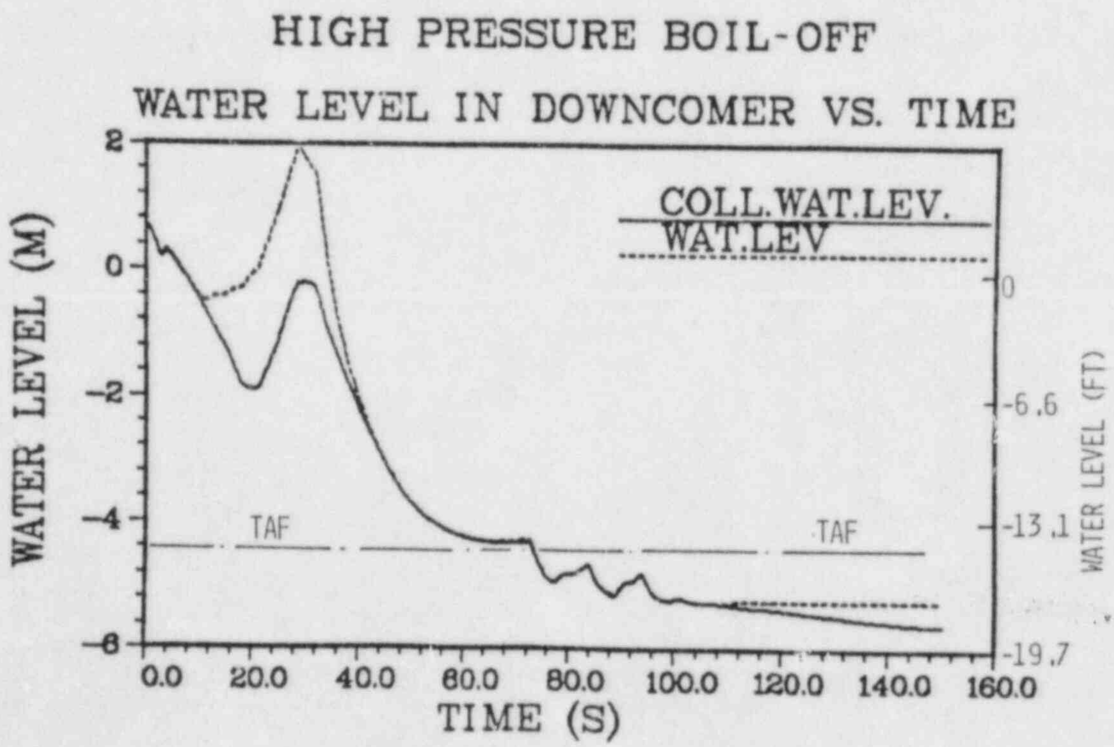



Figure 15. Downcomer water level predictions for Transient 3.

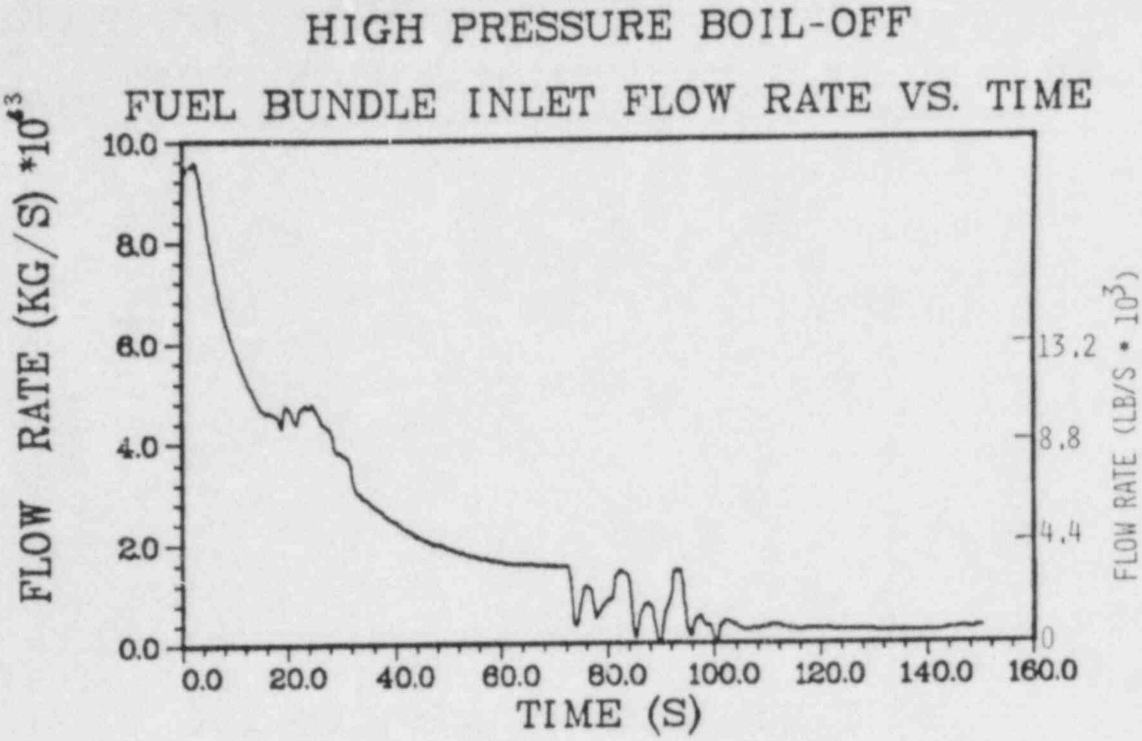



Figure 16. Core inlet flow rate for Transient 3.

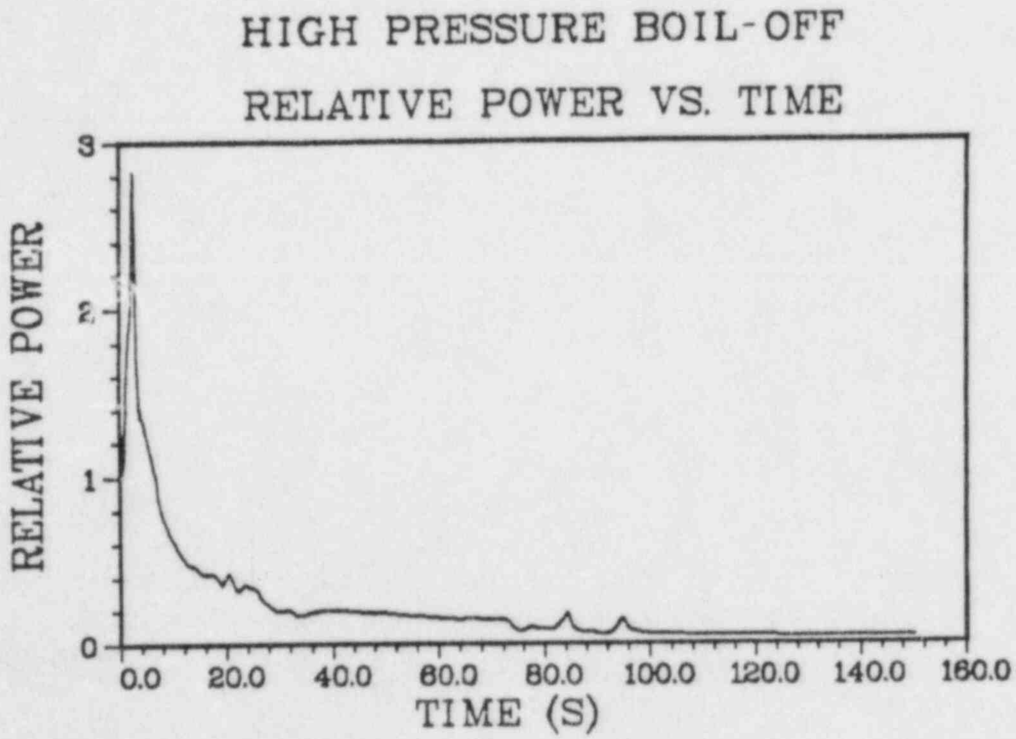



Figure 17. Reactor power history for Transient 3.

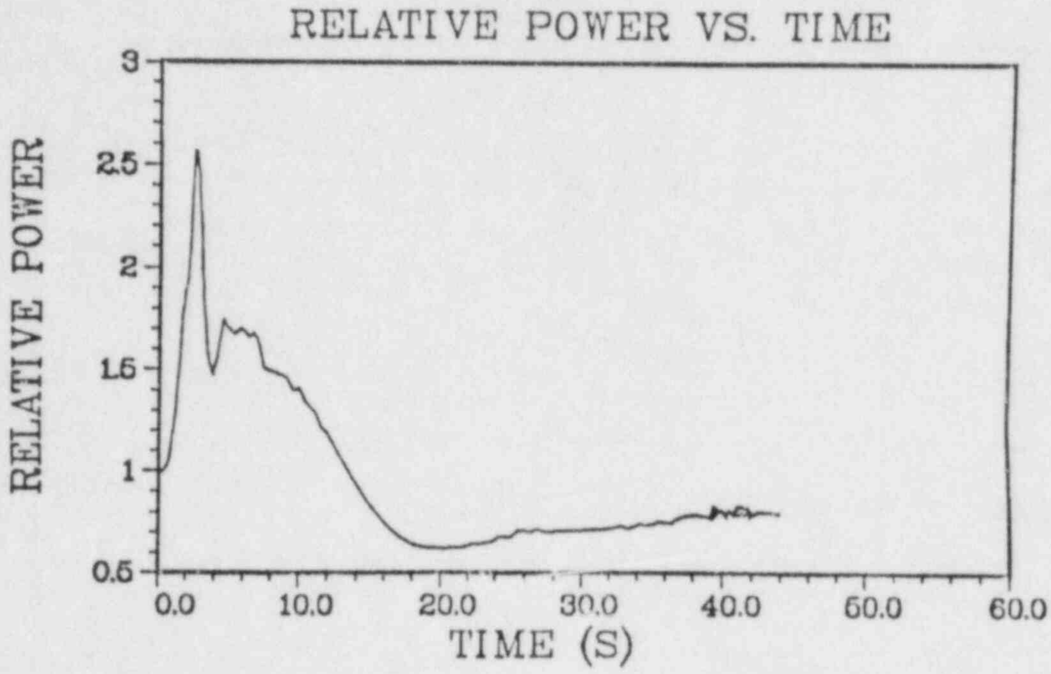



Figure 18. Relative power prediction for Transient 4.

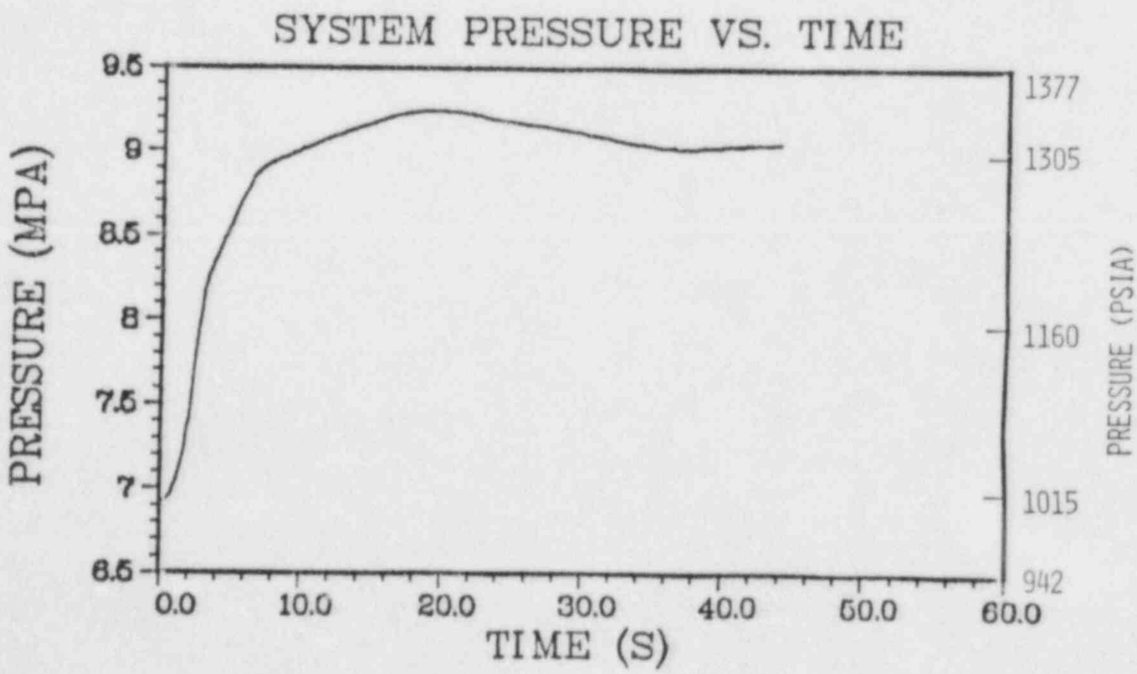



Figure 19. System pressure for Transient 4.

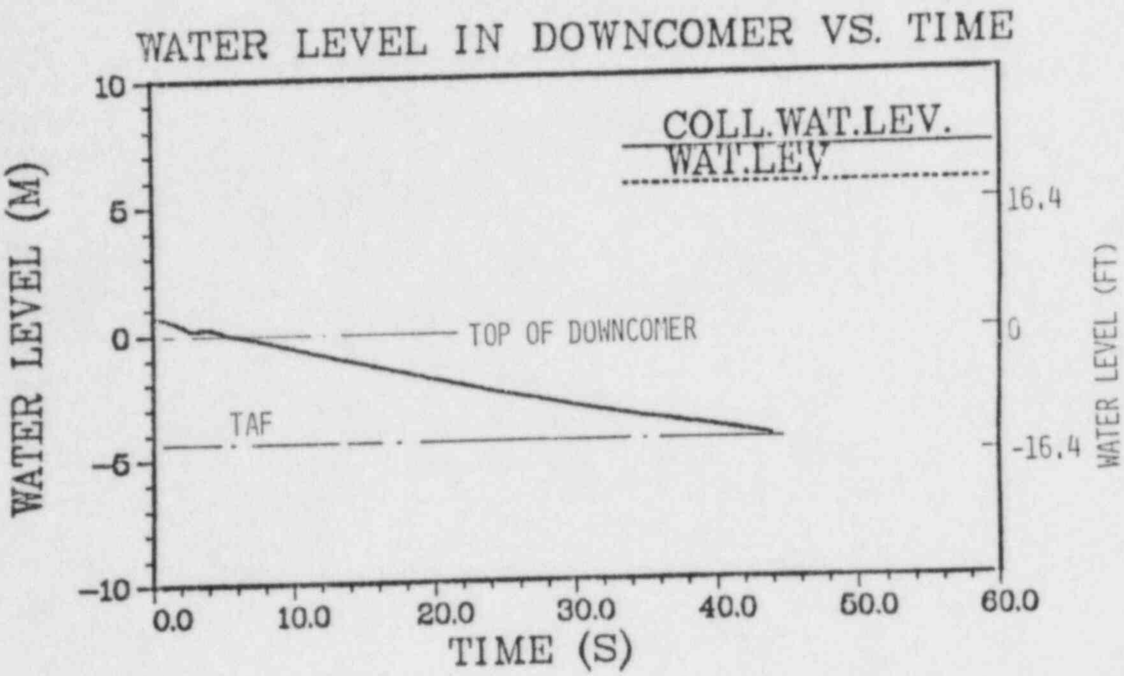



Figure 20. Downcomer water level for Transient 4.

# TRAC/MELPROG ANALYSES OF TMLB' TRANSIENTS IN OCONEE-1*

by

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## ABSTRACT

A severe degraded-core accident sequence of the Oconee-1 pressurized water reactor was simulated using the TRAC/MELPROG code, an integrated core-melt and thermal-hydraulics analysis code jointly developed by Sandia and Los Alamos National Laboratories. The TRAC/MELPROG code provides an integrated analysis of the behavior of core, vessel, and reactor-coolant systems during severe accidents. The severe accident sequence analyzed was a total loss of feedwater with failure of the emergency core-cooling system (a TMLB' sequence). The accident was calculated to 4775 s; at this time cladding had begun to melt and fuel rods near the top of the core had failed. Radial structural elements had also begun to fail and debris beds had formed in the lower areas of the core above the lower support plate. The calculation was terminated at 4775 s because of errors in the debris bed model. These errors have since been corrected, but we were not able to incorporate the corrections into the calculation because of restart incompatibility.

## INTRODUCTION

Realistic estimates of radioactive releases in the event of a severe accident require the use of a mechanistic calculational tool that models the entire primary system of a pressurized water reactor (PWR). This tool must begin with an accident initiator and determine the entire accident sequence through fuel failure and release of radioactivity from the primary system. The TRAC/MELPROG code under joint development by Sandia and Los Alamos National Laboratories will provide such a capability. The initial version of the TRAC/MELPROG code has been developed by linking TRAC-PF1/MOD1 (Ref. 1) (hereafter referred to as TRAC) developed at Los Alamos with the MELPROG/MOD0 (Ref. 2) code developed at Sandia and Los Alamos National Laboratories. MELPROG is coupled to TRAC to provide an integrated analysis of the behavior of core, vessel, and reactor-coolant systems during severe accidents. MELPROG treats core degradation and loss of geometry, debris formation, core melting, attack on supporting structures, slumping, melt-water interactions, and vessel failure. TRAC models the remainder of the primary and secondary systems including trip and control actions. In this paper, we discuss the first application of the TRAC/MELPROG code to a severe accident: a total loss of feedwater with failure

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*This work is supported by the US NRC Office of Nuclear Regulatory Research.

of the emergency core-cooling system (a TMLB' sequence) for the Oconee-1 reactor. The feedwater loss was assumed to be instantaneous. We analyzed the same event[3] with a prerelease version of TRAC/MELPROG, designated TRAC/MIMAS.

MODEL

Oconee-1 is a Babcock and Wilcox lowered-loop PWR and consists of a vessel, two steam generators (SGs), two hot legs, and four cold legs. For this first application of TRAC/MELPROG, the two hot and four cold legs have been combined into a single-loop representation of the primary cooling system (Fig. 1). Also modeled were the reactor coolant pumps (RCPs), loop seals, surge line, and pressurizer with relief valves. All thermal-hydraulic (flow) elements, including the vessel, were modeled in one dimension (1-D). MELPROG/MODO has the capacity to model the vessel's structural elements in two dimensions (2-D); this capability was used even though only a 1-D flow representation through the vessel was available. Structural elements modeled included the flow distributor plates, lower grid plate, lower support plate, upper tie plate, core barrel, baffle plate, formers, plenum cylinder, core-support shield, and core. The 2-D MELPROG structures model is depicted in Fig. 2.

RESULTS

After the feedwater was lost, the low-inventory, once-through SGs quickly boiled dry. With loss of the secondary heat sink, the primary began to pressurize and heat up. The power-operated relief valve (PORV) first opened at 406 s. The core-exit liquid temperature reached saturation at 1800 s, and boiling began in the core. By 3150 s there was insufficient liquid in the core to maintain the cladding temperatures near the saturation temperature, and cladding heatup began. The maximum cladding temperature at core-level 5 is shown in Fig. 3. Cladding oxidation began at 4245 s. The hydrogen-production and axial-oxidation-position histories are given in Fig. 4. The control rods near the top of the core began to fail at 4575 s. The cladding began to melt by 4650 s. Radial structural elements (baffle plate and formers) near the top of the core began to fail by melting at 4725 s. Debris beds began to form in the lower part of the core, above the lower support plate, by 4735 s. The core disruption and relocation that began near the top of the core proceeded to lower levels as the transient progressed, thereby increasing the height of the debris beds.

We concluded our transient calculation at 4775 s. We chose to terminate the calculation because errors were found in the debris bed model. These errors have since been corrected, but we were unable to incorporate these corrections for this calculation because the changes affect data storage and therefore restart compatibility.

In Fig. 5, we provide a snapshot showing the state of the primary system at 4750 s, just before the calculation was terminated. The liquid content of the primary was distributed among three locations, physically separated from each other by regions filled with steam. First, the vessel lower plenum, lower core, and lower downcomer were filled with water. Approximately the lower one-seventh of the core was liquid filled at the end of the calculation. Second, the primary side of the SG, the loop seal, contained water. However, the level of the water was below the cold-leg attachment to the RCP. Third, there was a small amount of water in the bottom of the pressurizer; this water did not drain back into the hot leg through the surge line because of a counter-current flow of steam, and in the later stages of the transient, hydrogen, passing through the pressurizer and out the PORV. The condition of the core at 4750 s is also detailed in Fig. 5. For the computational model, the core was divided into six equal-height levels and three equal-area radial rings. The fuel pins in levels three through six and all radial rings disintegrated following complete melting of the zirconium cladding and failure of the zirconium oxide layer in the cladding. The rubble resulting from pin disintegration was concentrated in level three; a smaller fraction of the rubble lodged in the fuel-pin stubs remaining in core level two. An even smaller fraction of the rubble was still in transition from core axial level four to the lower levels of the core. Several of the structures at the outer periphery of the core had failed (level 5). Specifically, the baffle plate and formers within axial level five had failed by complete melting; formers in levels three and four had also failed by complete melting. Thus, we have obtained a first-order view into the dimensionality of damage within the vessel.

We note that the phenomena predicted with 1-D flow models do not fully describe the expected phenomena in the core region. Specifically, it is anticipated that natural-circulation phenomena will influence the course of the transient. We have completed preliminary calculations with a MELPROG version that includes a 2-D flow analysis capability.[4] A strong natural-circulation flow is predicted in the upper plenum. A weaker, but still significant, natural-circulation flow penetrates into the core region, retards the rate of temperature increases in the core, and acts to delay cladding melting.

## CONCLUSIONS

An integrated TRAC/MELPROG calculation of a TMLB′ sequence in Oconee-1 has been performed. This transient was calculated to 4775 s; at this time cladding had begun to melt and fuel rods near the top of the core had failed. Radial structural elements had also begun to fail. Debris beds had formed in the lower areas of the core above the lower support plate. We terminated the calculation because the MELPROG/MOD0 debris models failed; these models have been corrected but cause the updated TRAC/MELPROG code to be restart incompatible with the code version used to calculate the transient to 4775 s.

We note that the code used consisted of TRAC-PF1/MOD1 and MELPROG/MOD0 linked together to function as a single code. This code version is limited because it is capable of performing only 1-D fluid-flow calculations. We believe that at least a 2-D flow model of the vessel is required if we are to accurately represent the phenomena occurring in the vessel after the top of the core is uncovered. A 2-D flow analysis capability is under development and will be available in 1986 when MELPROG/MOD1 is released.

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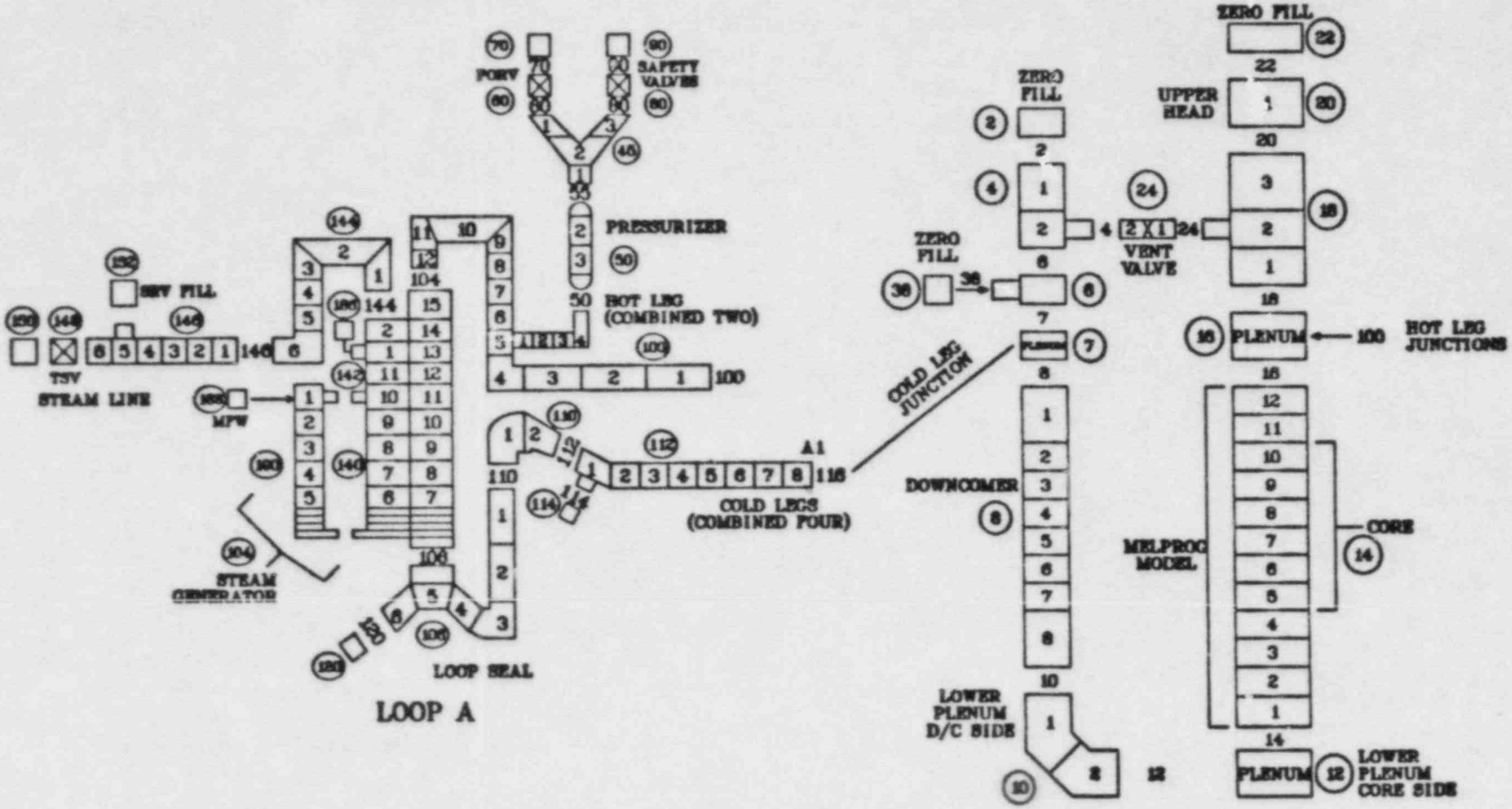


Fig. 1.
TRAC/MELPROG noding diagram for Oconee-1 TMLB′ transient.
MELPROG is responsible for the core component.

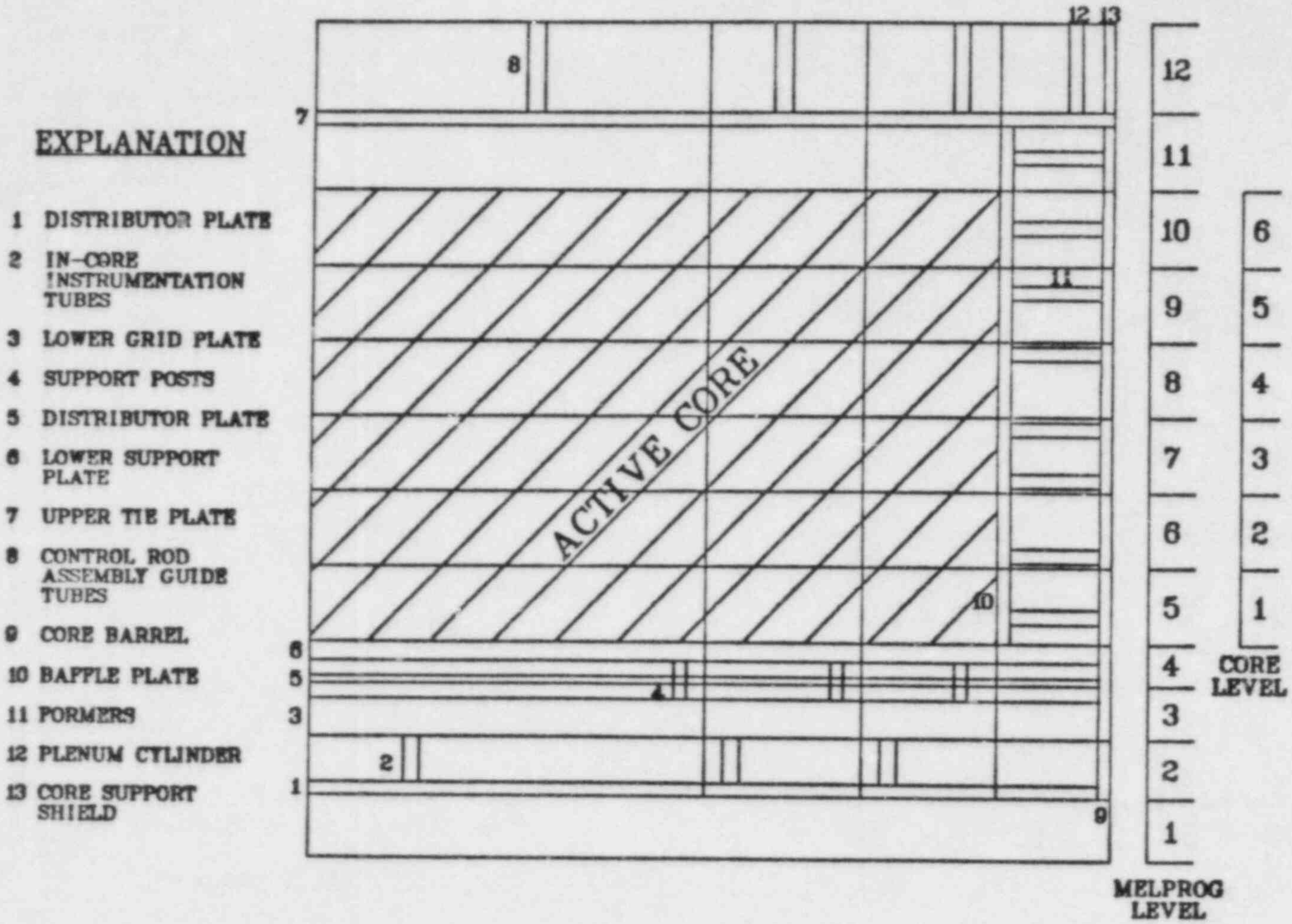


Fig. 2.
Detailed MELPROG noding diagram showing the structures that were modeled.

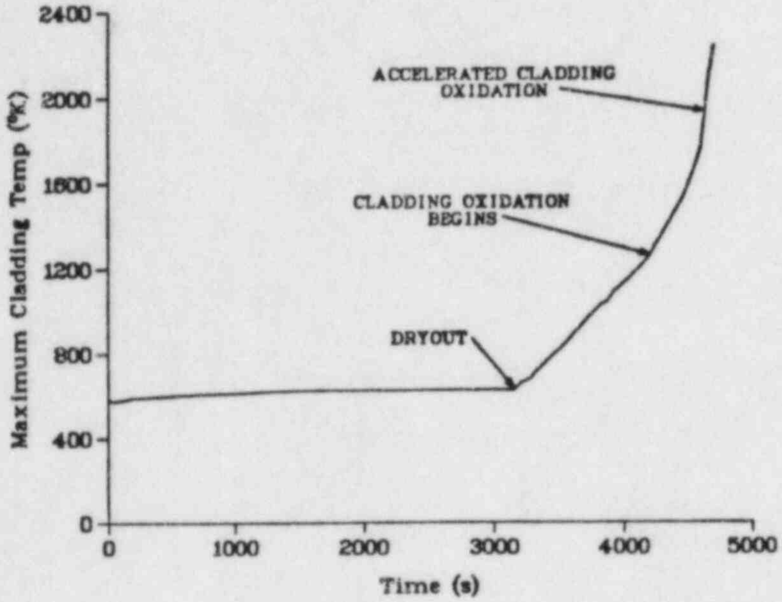


Fig. 3.
Maximum cladding temperature as determined by MELPROG.

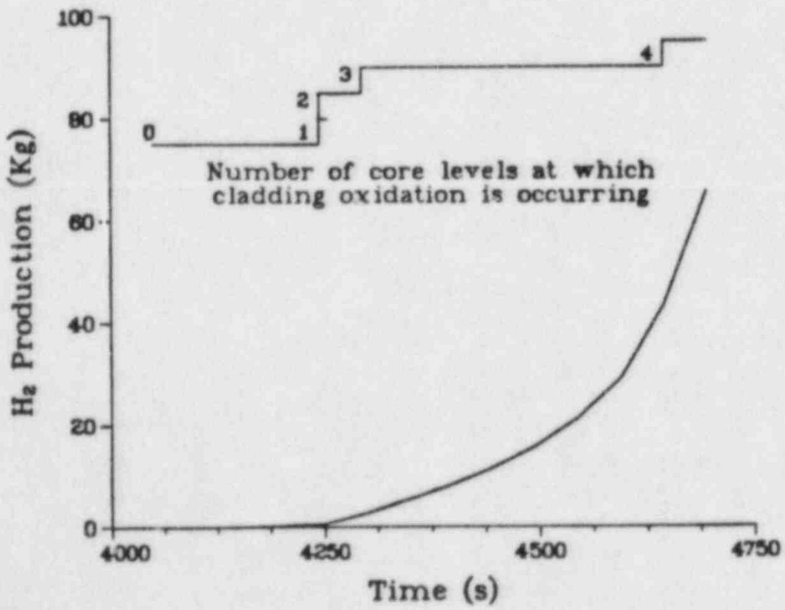


Fig. 4.
Hydrogen production and axial position of oxidation.

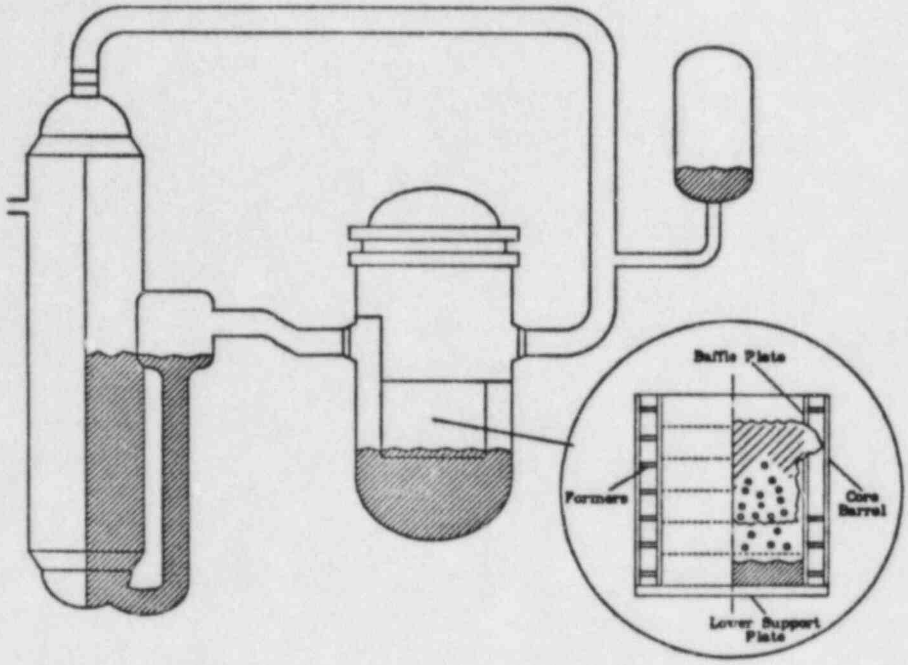


Fig. 5.
Primary system at 4750 s into the TMLB' transient.

# ATWS ANALYSIS FOR BROWNS FERRY NUCLEAR PLANT UNIT 1[a]

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## ABSTRACT

Analyses of postulated Anticipated Transients Without Scram (ATWS) were performed at the Idaho National Engineering Laboratory (INEL). The Browns Ferry Nuclear Plant Unit 1 (BFNP1) was selected as the subject of this work because of the cooperation of the Tennessee Valley Authority (TVA). The work is part of the Severe Accident Sequence Analysis (SASA) Program of the U.S. Nuclear Regulatory Commission (NRC). A Main Steamline Isolation Valve (MSIV) closure served as the transient initiator for these analyses, which preceded a complete failure to scram. Results from the analyses indicate that operator mitigative actions are required to prevent overpressurization of the primary containment. Uncertainties remain concerning the effectiveness of key mitigative actions. The effectiveness of level control as a power reduction procedure is limited. Power levels resulting from level control only reduce the Pressure Suppression Pool (PSP) heatup rate from 6 to 4°F/min.

## INTRODUCTION

The topic of this paper is the analysis conducted at the Idaho National Engineering Laboratory (INEL) of Anticipated Transients Without Scram (ATWS) in a Boiling Water Reactor (BWR). With the cooperation of the Tennessee Valley Authority (TVA), the subject plant is the Browns Ferry Nuclear Plant Unit 1 (BFNP1). The work is part of a cooperative effort coordinated by the Severe Accident Sequence Analysis (SASA) Program of the U.S. NRC. Work performed at the Oak Ridge National Laboratory (ORNL)[1] and Brookhaven National Laboratory (BNL) is also part of the BWR SASA effort on ATWS. The objectives of this task include establishing the methodology for and performing comprehensive deterministic analyses of severe accidents, determining accident event progression, evaluating the effects of plant safety

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systems and operator actions on accident progression, and defining requirements for improved analytical methods or for experiments to resolve uncertainties.

An ATWS is a postulated operational occurrence which is followed by a failure to scram the reactor. Previous probabilistic analyses[2] have asserted that the ATWS at BFNP1 is included in a group of dominant transients relative to core damage frequency. Although low in probability, ATWS accidents are of concern because they could lead to core damage and fission product release to the environment. The ATWS studied here is initiated by a Main Steamline Isolation Valve (MSIV) closure. MSIV closure isolates the Reactor Pressure Vessel (RPV) from the main condenser. Steam produced in the RPV is discharged to the Pressure Suppression Pool (PSP), creating a threat to containment unless power can be reduced to decay heat level. The outcome of the transient is governed by the ability of plant systems and operator actions to maintain core cooling and containment integrity until the accident can be controlled.

Consistent with SASA objectives, the MSIV closure ATWS at BFNP1 was studied by developing an analysis methodology. Using probabilistic methods, a sequence event tree (SET)[3] was developed which described the many paths (sequences) that an ATWS could follow in terms of plant systems and operator actions. Analysis of the most probable sequences yielded insight to the possible consequences resulting from a MSIV closure ATWS. Results from the analyses are presented in Reference 4. This paper will highlight pertinent topics from the analyses.

## RESULTS

Deterministic analyses were performed using two best estimate computer codes. RPV response was calculated with RELAP5/MOD1.6.[5] Besides the RPV and associated internals, RELAP5 was used to model the feedwater line, main steamlines, Standby Liquid Control System (SLCS), Control Rod Drive (CRD) system, and the Emergency Core Cooling (ECC) systems. The RELAP5 model was extensively assessed with plant specific transient data and data from the Full Integral Simulation Test (FIST) Facility.[6] The primary containment, specifically the drywell, and PSP, was modeled with CONTEMPT/LT-028.[7] RELAP5 provided boundary conditions to CONTEMPT in the form of mass and energy rates through the Safety Relief Valves (SRVs). CONTEMPT in turn provided event timings back to RELAP5. Figure 1 illustrates the information exchanged between the two codes.

Events occurring during the early portion of a MSIV closure ATWS (Table 1) are well defined. MSIV closure causes a rapid pressurization of the RPV because the core continues to produce steam at near rated conditions. Increased pressure collapses voids in the core which provides positive reactivity feedback. Increased power results, which increases the core steaming rate. The increasing pressure causes the recirculation pumps to trip (at 1135.0 psia), and the SRVs to open.

MSIV closure isolates the steam supply of the feedwater turbines from the RPV. As the feedwater turbines and pumps rapidly coast down, turbine-driven feedwater is lost. Continued core steaming causes the vessel downcomer level to decrease. When lo-lo level (476.0 in.) is reached, HPCI and RCIC systems are activated. Taking suction from the CST, HPCI pumps 5000 gpm and RCIC pumps 600 gpm of high pressure makeup into the RPV via the feedwater line. For the purposes of these analyses, it was assumed that a scram signal was generated but no control rods were inserted. Following the scram signal, 112 gpm of water was pumped into the RPV by the CRD system. Figure 2 illustrates the flow alignment described above.

Following recirculation pump trip, the reactor stabilizes in a natural circulation mode. Core power is determined from the mass flow rates and enthalpies of the injected water. The resulting power with HPCI, RCIC, and CRD injection is ~30% of rated. This causes a steaming rate of ~21% of normal steamline flow to be relieved to the PSP. The difference in core power and RPV steaming rate is accounted for by the heating of the highly subcooled ECC fluid.

The RPV stabilizes to a quasi steady state condition in less than 100 s after transient initiation. Steam produced in the core is relieved to the PSP through the cycling of two to four SRVs. As the steam is condensed in the PSP, the PSP heats up at the rate of ~6°F/min.

Subsequent events are dependent on the interaction of plant systems and operator actions. A simulation was performed to assess the transient progression during automatic plant operation (no operator actions). Results from that simulation indicated that drywell failure pressure (132 psia) would be reached ~45 minutes after transient initiation.

Ultimately, termination of an ATWS progression can only be accomplished by shutting the reactor down. The operator must either insert control rods or borate the core to effect a hot shutdown. Other actions are intended to extend the transient chronology to allow time for control rod insertion and/or boron injection to become effective. The main objectives of those actions are to maintain adequate core cooling and to reduce core power. The importance of reducing core power lies in the fact that without reactor shutdown, the containment could eventually overpressurize. Reducing core power also reduces the core steaming rate and the corresponding PSP heatup rate. By reducing the PSP heatup rate, the time to containment pressurization is extended.

Analyses of postulated ATWS in BWRs involve significant uncertainties. Among those are the effectiveness and timing of operator actions. When and how the operator would react to an ATWS is extremely important to the transient outcome. The effectiveness of individual control rod insertion is dependent on several factors that are difficult to quantify. Currently, the capability does not exist to mechanistically calculate the transport and effectiveness of sodium pentaborate (from the SLCS).

At BFNP1, injection of the SLCS is through a single sparger located in the lower plenum just below the core plate. This asymmetrical injection raises questions concerning the boron solution being transported uniformly into the core. Because the boron solution has a specific gravity of 1.1 and it is highly subcooled when it enters the RPV, the solution has the potential for settling in the lower plenum particularly during low core flow conditions. The analyses performed at the INEL used a bounding approach in modeling boron injection. To determine maximum effectiveness, the boron solution was calculated to move isotropically with the liquid in the vessel. This provided results based on ideal conditions. The other bounding solution was obtained by assuming minimum effectiveness. This was done by calculating the boron worth to be zero until an amount sufficient for hot shutdown was injected. At that time, the total amount of boron was placed in the lower plenum and then transported into the core.

During an ATWS, the reactor power is determined by the energy and mass flow rate of the fluid injected into the RPV. In other words, during quasi steady state conditions if the injected flow rate and energy are known, the reactor power can be easily calculated. Controlling parameters other that flow rate can make the determination of reactor power more difficult. For instance, proposed Emergency Procedure Guidelines (EPGs)[8] advocate a procedure termed level control for the purpose of reducing core power and the resulting steaming rate to the PSP. Level control involves the lowering of the downcomer water level to the top of the active fuel (TAF) by throttling makeup injection systems. Analysis of level control involves complicated thermal hydraulic/neutronic feedbacks. Calculations at the INEL result in a predicted power level of approximately 17%, when downcomer level is at TAF and the RPV is at 1000 psia. This value agrees closely with previously published Electric Power Research Institute (EPRI) results[9,10] and with RAMONA-3B calculations[11] done at BNL as part of the SASA Program.

Thus, if the operator takes level control, the reactor power is predicted to be reduced from approximately 30% to 17% of rated. The net result of that power reduction is to reduce the PSP heatup rate from 6 to 4°F/min. This is illustrated on Figure 3.

Figure 3 in effect shows the marginal effect of level control and core boration. The automatic response of the reactor to an MSIV closure ATWS is such that the PSP heats up at a rate of ~6°F/min. This heatup rate is based on the enthalpy balance across the reactor induced by the full flow of the HPCI and RCIC systems. In these simulations, it was assumed that the operator took level control early, just two minutes after transient initiation, and that his actions tended to maximize the worth of this mitigative action. Thus, the delay in PSP time-to-temperature obtained was maximized. The effect of level control is not noteable unless it is considered in terms of the time necessary to inject an amount of boron into the reactor necessary to effect a hot shutdown.

The simulated effectiveness of the SLCS was minimized by assuming that all the boron injected into the reactor stratified in the lower plenum. This assumption was modeled by injecting no boron into the vessel until such a time as 265 lb, an amount sufficient to effect a hot shutdown, would have been injected. At this time, the simulation was stopped, and 265 lb was placed in the lowermost node of the lower plenum. The simulation was then restarted assuming that the operator had terminated level control, and was refilling the reactor with the HPCI system. The time-to-hot shutdown was calculated using both 50 and 86 gpm capacity SLCS systems.

The results of these simulations are shown on Figure 3, and indicate that the maximum potential effectiveness of level control has a worth roughly equivalent to increasing the SLCS capacity from 50 to 86 gpm by doubling the number of on-line pumps. As indicated by the figure, level control reduces the PSP heatup rate from 6 to 4°F/min. With a 50 gpm SLCS capacity it would take 1585 s to inject 265 lb of boron, and with 86 gpm it would take 980 s. The net effect of level control in these cases is to affect a lower PSP temperature at the time of shutdown.

Analysis of the physical mechanisms governing level control indicates that the relative worth of it cannot be easily enhanced. Figure 4 shows the results of a series of RELAP5 simulations designed to give reactor power as a function of collapsed downcomer liquid level. When the downcomer level is above the top of the active fuel (TAF), but below the top of the upper plenum, reactor power is fairly insensitive to the level. There is a wide plateau on which the reactor power stagnates at ~17% (assuming that the inlet of the core is saturated). Below this plateau, reactor power decreases rapidly with level, but the accuracy of level instrumentation at these reduced levels can be questioned. Thus, it becomes apparent that no further reduction in power can be accomplished by controlling the downcomer liquid level.

Complementary guidance for, and a possible alternative to, level control can be given to the operator by specifying before hand the HPCI flow necessary to maintain the state of the reactor on the level/flow plateau. Table 2 shows some typical values of flows and powers computed from a enthalpy balance, and downcomer levels computed using RELAP5. The numbers indicate that by reducing HPCI flow, the reactor power can be reduced significantly. A corresponding reduction in PSP heatup rate could also be accomplished.

## CONCLUSIONS

This article is concluded with an overview of these contents. The primary mechanism of damage occurring from a postulated ATWS at the Browns Ferry Nuclear Plant, a BWR/4 with a MARK I containment system, is primary containment overpressure. This overpressure can be prevented by shutting the reactor down with the SLCS system or by control rod insertion. Analysis of the actions and mechanisms of SLCS boration of the core indicate that, in conjunction with level control, the reactor can be shut down before the PSP reaches saturation if the operator acts in a timely and effective manner.

Level control reduces reactor power from 30 to ~17% of rated. That result is obtained with the level at TAF, the RPV at 1000 psia, and saturated core inlet conditions. Reducing the power to 17% slows the PSP heatup rate from 6 to 4°F/min.

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TABLE 1. MSIV CLOSURE ATWS SEQUENCE OF EVENTS--FIRST 80 SECONDS[a]

| Time (s) | Value | Event |
|---|---|---|
| 0.0 | -- | Transient initiation, MSIVs begin to close. |
| 0.4 | 10% | MSIV fractional area has decreased 10% causing scram signal. |
| 2.67 | 104.7% | Jet pump discharge mass flow peaks. |
| 2.71 | 253.0% | Peak reactor power. |
| 3.03 | 110.0 psi/s | Maximum rate of change of steam dome pressure, SRVs begin to open. |
| 3.26 | 1135.0 psia | Recirculation pump trip signal on high steam dome pressure. |
| 3.79 | -- | Recirculation pumps trip, begin flow coastdown. |
| 5.0 | -- | MSIV completely closed. |
| 6.7 | 2582°F | Peak fuel temperature. |
| 6.7 | 82% | SRV flow peaks at 82% of normal steaming. |
| 7.3 | 608.9°F | Peak cladding temperature. |
| 10.5 | 1272 psia | Peak steam dome pressure. |
| 34 | 476.0 in. | Downcomer level reaches lo-lo level, HPCI and RCIC actuate. |
| 78 | 110°F | PSP temperature reaches 110°F, SLCS injection called for by EPGs. |
| 79 | 5600 gpm | HPCI, RCIC at full flow. |

a. Event timings as predicted by RELAP5/MOD1.6.

TABLE 2. APPROXIMATE FLOW AND POWER LEVELS DURING A BWR ATWS

| Makeup Flow (gpm) | Reactor Power (%) | Downcomer Level (ft) | PSP Heatup Rate (°F/min) |
|---|---|---|---|
| 5600 | ~30 | ~45 | ~6 |
| 3400 | 17.7 | 29.6 | ~4 |
| 3230 | 16.7 | 29.0 | ~4 |
| 1428 | 8.7 | 27.3 | ~2 |

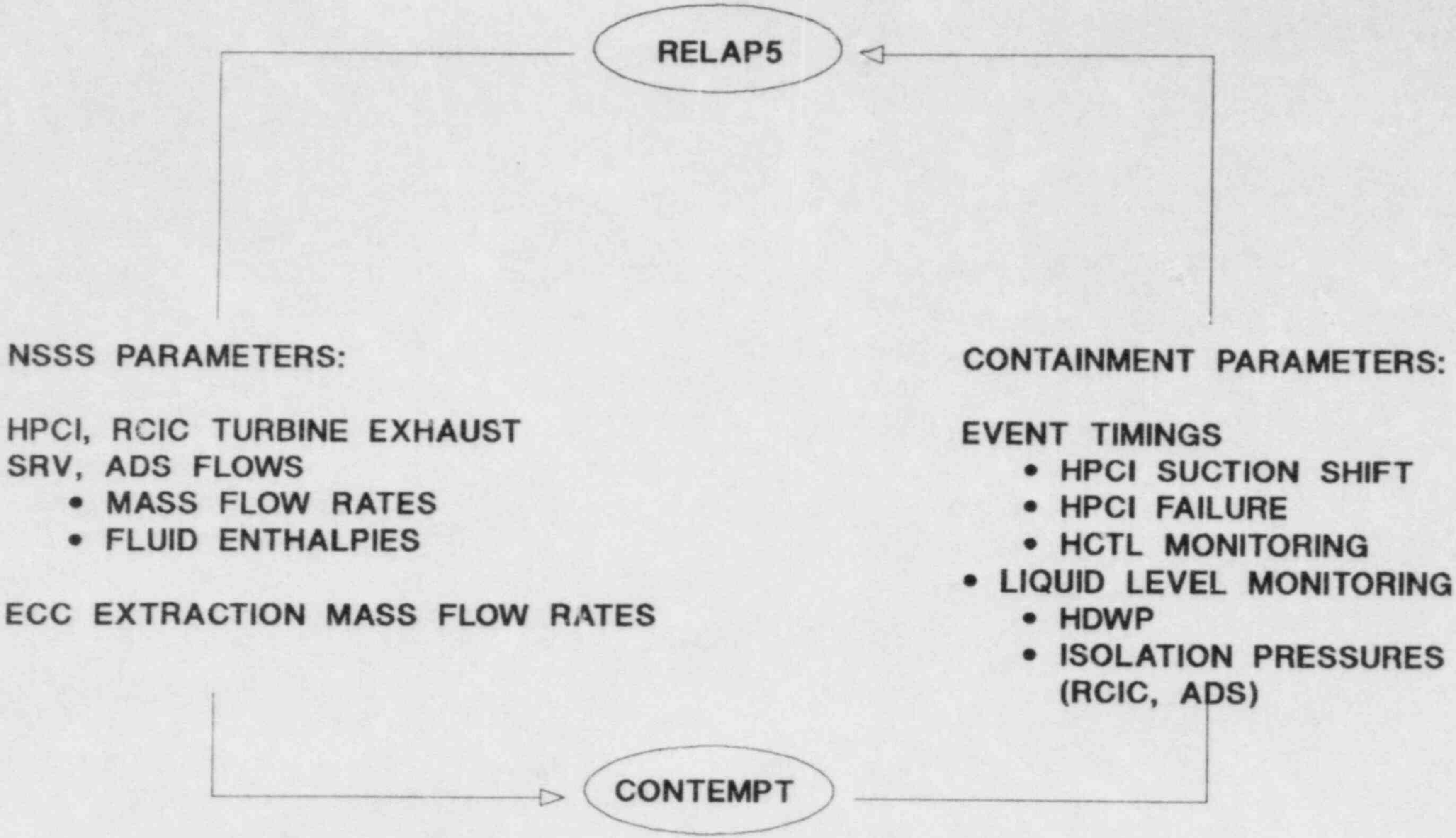


Figure 1. Information exchange between RELAP5 and CONTEMPT.

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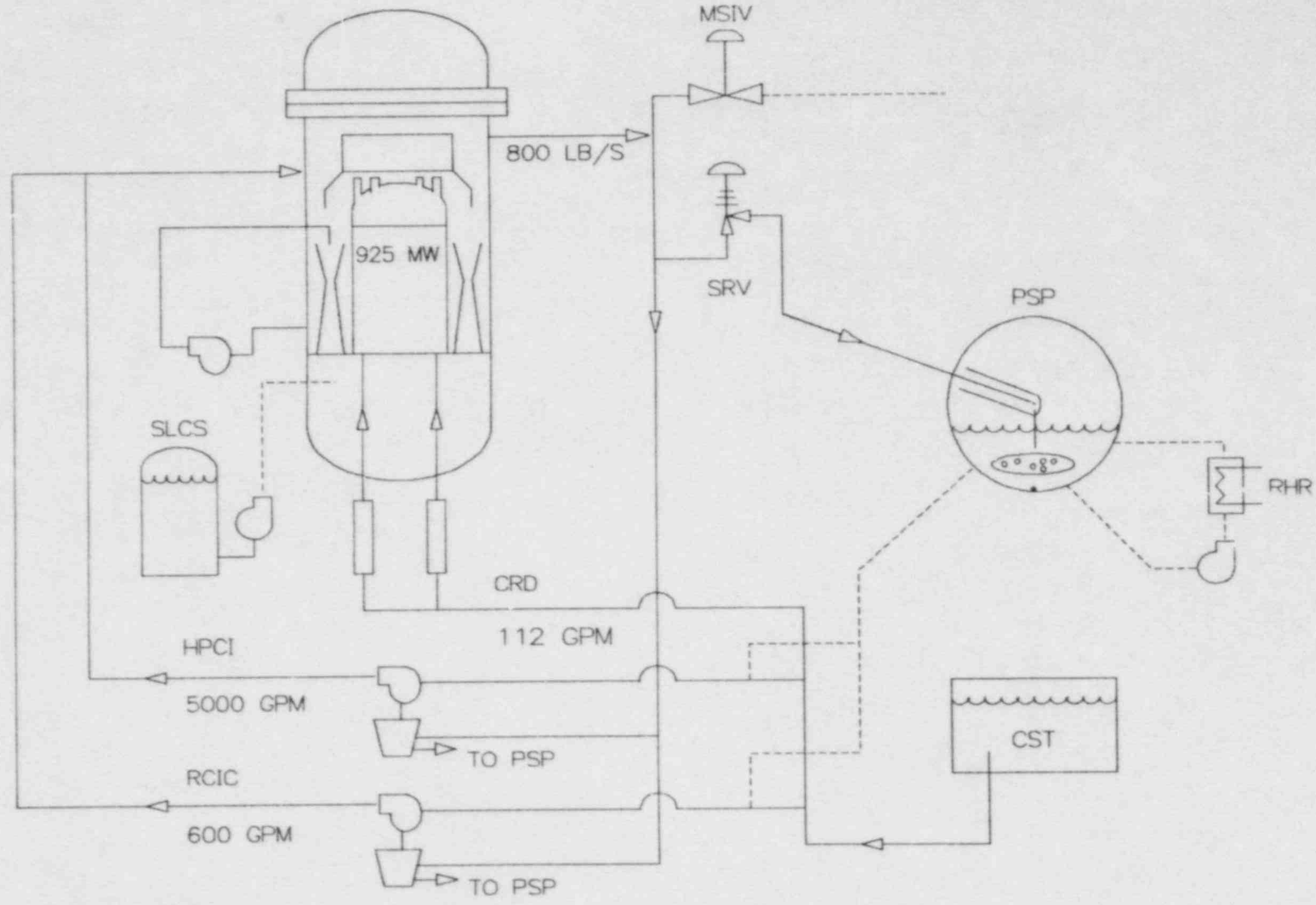


Figure 2. A quasi steady state condition is established early in the transient.

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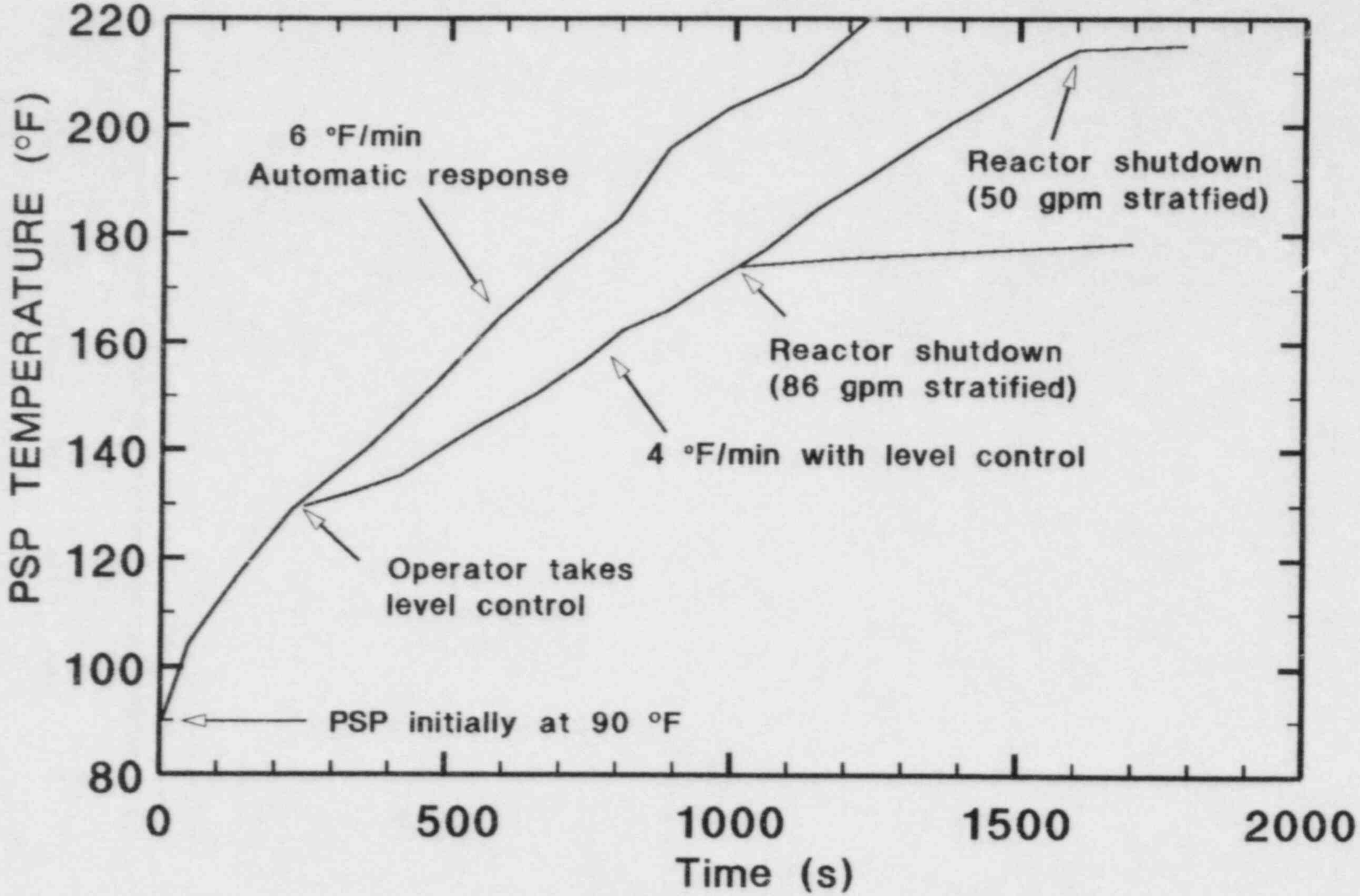


Figure 3. Operator mitigative actions can reduce and terminate PSP heatup.

EEJ00770

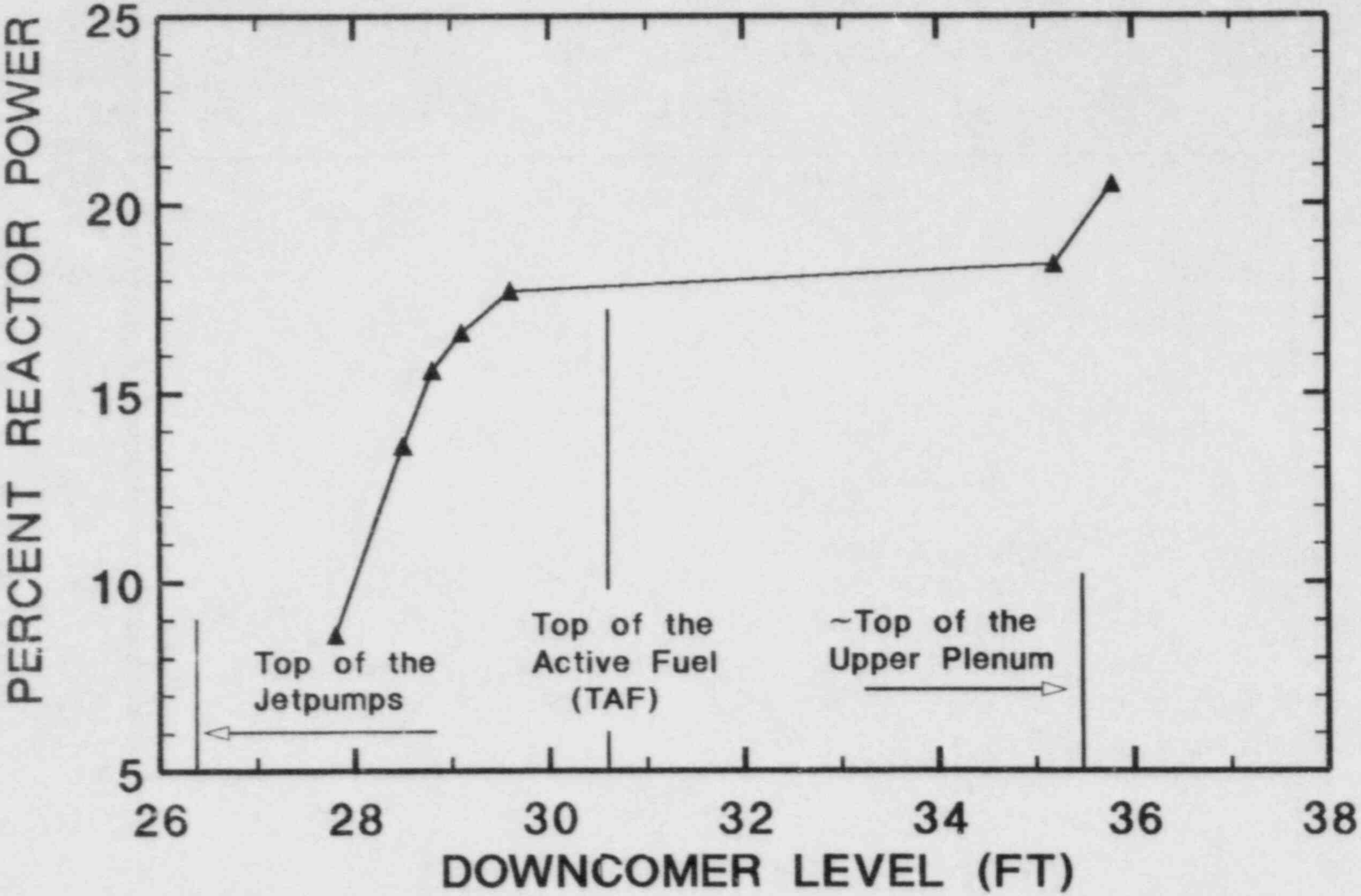


Figure 4. Reactor power vs downcomer liquid level in steady state operation.

EEJ00769

# ANALYSIS OF FEEDWATER TRANSIENT INITIATED SEQUENCES FOR THE BELLEFONTE NUCLEAR PLANT[a]

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## ABSTRACT

Four feedwater transient initiated sequences for the Bellefonte Nuclear Plant were analyzed. The sequences were evaluated to determine if core damage would result. Calculations were performed with the RELAP5/MOD2 computer code until either cladding oxidation or long term core cooling was obtained. The analyses show that a total loss of power and auxiliary feedwater (TMLB' sequence) results in core damage. The addition of a single HPI pump was shown to provide adequate core cooling.

## INTRODUCTION

A series of feedwater transient initiated sequences for the Bellefonte nuclear power plant has been analyzed at the Idaho National Engineering Laboratory (INEL). The analyses are part of the Severe Accident Sequence Analysis (SASA) Program sponsored by the United States Nuclear Regulatory Commission. The analyses support the Tennessee Valley Authority (TVA) development of a probabilistic risk assessment (PRA) for Bellefonte. The INEL analyses were performed with the RELAP5/MOD2 computer code to evaluate the thermal-hydraulic response of Bellefonte to these postulated transient scenarios. The INEL analyses provide information regarding time to core damage if it is predicted to occur for the transient sequences evaluated. The following sections discuss the feedwater transient initiated sequences evaluated, the Bellefonte plant and the RELAP5 model used, the analytical results obtained, and the conclusions reached. This document also presents details of current and planned SASA analyses for Bellefonte.

## TRANSIENT DESCRIPTIONS

Four feedwater transient initiated sequences were to be evaluated. The transients were selected by TVA for resolution of PRA issues and are:

[a]Work sponsored by the U. S. Nuclear Regulatory Commission, Office of Nuclear Regulatory Research, under DOE Contract No. DE-AC07-76IDO1570.

| | |
|---|---|
| Transient 1.A | Loss of offsite power with concurrent loss of onsite power and failure to provide emergency feedwater (TMLB') sequence; |
| Transient 1.B | Transient 1.A with additional loss of the power operated relief valve (PORV) and with one high pressure injection (HPI) pump operational; |
| Transient 1.C | Transient 1.A with one HPI pump operational; |
| Transient 1.D | Transient 1.A with two HPI pumps operational. |

The initial transient (Transient 1.A) is initiated at full power by the complete loss of all station A. C. power. The loss of power results in a simultaneous turbine trip, reactor scram, primary reactor coolant pump trip, and main feedwater valve closure initiation. Turbine trip isolates the steam generators from the turbines by closing the turbine stop valves. The isolated steam generator secondaries are provided pressure relief via the modulating atmospheric dump valves and the steam line safety relief valves (SRVs). The primary system pressure relief is provided by the PORV and the primary system SRVs. Emergency core cooling systems, auxiliary feedwater systems, and primary makeup and letdown systems are not operational during this transient.

Transient 1.B assumed the same sequence of events as Transient 1.A with two additional conditions imposed. The PORV is assumed unavailable (either failed closed or the upstream block valve is closed) so that primary system pressure relief is through the SRVs only. It is further assumed that a single HPI pump is online and injecting equally into the four cold legs. The HPI pump is turned on when the primary system initially reaches the PORV opening pressure of 2310 psia (15.93 MPa).

Transient 1.C assumed the same sequence of events as Transient 1.B, but with the PORV assumed operational. The initiation of a single HPI pump in this transient is coincident with the initial opening of the PORV.

Transient 1.D is identical to transient 1.C with full HPI delivery assumed (two pumps). Transient 1.D was to be run only if Transient 1.C resulted in core damage.

The transients described were to be evaluated until either a success or a failure criterion had been met. Success was defined as the establishment of adequate long term core cooling. Failure was defined to occur if fuel cladding surface temperatures exceeded the initiation temperature for potentially significant cladding oxidation. The temperature used for the failure criteria was 1340 $^{o}$F (1000 K).

## COMPUTER CODE DESCRIPTION

The feedwater transients were analyzed utilizing the RELAP5/MOD2 computer code[1]. RELAP5 is an advanced one-dimensional, two-fluid, nonequilibrium computer code utilizing a full six equation hydrodynamic model providing continuity, momentum and energy equations for each of two phases within a control volume. The energy equations contain source terms which couple the hydrodynamic model to the heat structure conduction model by a convective heat transfer formulation. The code contains special process models for critical flow, abrupt area changes, branching, cross-flow junctions, pumps, valves, core neutronics, and control systems.

## BELLEFONTE PLANT AND INPUT MODEL DESCRIPTION

Bellefonte is a Babcock and Wilcox design pressurized water reactor with a thermal power rating of 3600 MW. The design incorporates two primary coolant loops connected to the reactor vessel, each consisting of a hot leg, a once-through steam generator, two pump suction legs, two reactor coolant pumps, and two cold legs. The HPI system injects into the primary system at the cold legs just downstream of the reactor coolant pumps. A pressurizer is connected to one of the hot legs with a surge line. The RELAP5 input model simulated the two hot legs and four cold legs explicitly and represents the primary and secondary systems with 187 volumes, 194 junctions, and 185 heat structures. Further details of the RELAP5 model of Bellefonte are given in Reference 2.

The Bellefonte RELAP5 model was initialized to steady state conditions reflecting anticipated operating conditions at 100% of rated core power. Table 1 compares the values of selected plant parameters as calculated by RELAP5/MOD2 to those presented in the Bellefonte Final Safety Analysis Report[3] for full power steady state operation. Differences between the desired (anticipated) and computed values are well within the uncertainty in the plant operating parameters.

## RESULTS

The following describes the results of the feedwater transient initiated sequences for Bellefonte. Transient 1.A was the only sequence evaluated that resulted in heatup of the fuel cladding to potentially significant oxidation temperatures. Transients 1.B and 1.C were terminated after analyses indicated one HPI pump was sufficient to provide adequate long term core cooling. The core cooling is expected to continue until the borated water storage tank (BWST) that supplies the HPI system empties 20 to 25 hours after transient initiation. Transient 1.D was not analyzed since it is a repeat of Transient 1.C with a higher HPI injection rate, with the higher injection rate also providing adequate long term cooling.

The sequence of events for the three transients analyzed is summarized in Table 2. The events occurring during the initial 3.1 s are boundary conditions common to all three transients. Transient 1.A produced core heatup above coolant saturation temperature 1930 s after transient initiation with significant oxidation temperatures reached 770 s later. Neither Transient 1.B nor 1.C resulted in core heatup but, instead, showed that a single HPI pump can provide adequate core cooling by 5000 s with SRVs and by 3500 s with both PORV and SRVs providing primary system pressure relief.

Figure 1 presents the primary system pressure response for Transient 1.A. The pressure decreased initially because the heat transfer to the secondary coolant system was greater than the heat added in the core. The steam generators dried out quickly, and the pressure increased to the PORV opening pressure 230 s after transient initiation. The PORV cycled for about 650 s, after which it could no longer relieve the system pressure. The reactor coolant system pressure then increased to the opening pressure of the SRVs. The SRVs were able to control the primary system pressure. The slower depressurization after the second SRV opening was caused by boiling in the reactor coolant system. After about 2500 s, the liquid level in the core and the boiling rate were low enough that the PORV alone was able to relieve the pressure.

The calculated fuel cladding surface temperatures near the bottom, middle, and top of the core are shown for Transient 1.A in Figure 2. The temperatures show that the core dryout was from the top down. Cladding heatup was observed to begin 1930 s after transient initiation in the upper core with cladding temperatures exceeding 1340 $^{o}F$ (1000 K) at 2700 s.

The calculated primary system pressure response to Transient 1.B is shown in Figure 3 and to Transient 1.C in Figure 4. Transient 1.B showed repressurization to the SRV opening pressure 330 s after transient initiation. The SRVs then cycled repeatedly for the remainder of the transient. Transient 1.C showed a response similar to that observed in Transient 1.A with the timing to initial opening of the SRVs delayed by cooling provided by the HPI system. The SRV opening pressure was reached 2050 s after transient initiation and the valves cycled seven times. Following 3400 s, pressure relief was provided by the PORV, which was full open from 900 s on. Transient 1.B provides more challenges to the SRVs which, over the course of the 20 to 24 hours before the BWST emptied, could fail.

The comparison of the primary system mass inventory for Transients 1.B and 1.C is shown in Figure 5. The comparison shows the difference between mass injected (HPI) and mass expelled (relief valve[s]) for the two transients. The combination of PORV and SRVs produced a more rapid decrease in system mass in Transient 1.C until SRV cycling ended at 3400 s.

HPI flow was then able to exceed PORV flow and the primary system began to refill. In Transient 1.B, the primary system began refilling after 5000 s when the HPI flow exceeded the flow being expelled through the cycling SRVs.

## CONCLUSIONS

The analyses show that Transient 1.A, the TMLB' sequence, resulted in core dryout and fuel cladding heatup to rapid oxidation initiation temperatures. Availability of a single HPI pump is sufficient for core cooling regardless of PORV availability until the BWST empties between 20 and 24 hours after transient initiation. The comparison between Transients 1.B and 1.C show that PORV availability results in fewer challenges to the SRVs and, therefore, less chance for their failure.

Further analyses of the Bellefonte plant are currently underway. A series of small break transient initiated sequences are being analyzed to further support the Bellefonte PRA development. These transients are characterized by small cold leg break loss-of-coolant accidents with additional failures of auxiliary feedwater, HPI systems, low pressure injection systems, and accumulators. The TMLB' sequence described above is currently being analyzed through the core damage phase of the transient. The analysis is being performed to determine the interaction between the core and the balance of the primary system. The analysis is being performed with the RELAP5/SCDAP integrated computer code. The code provides best estimate coupling of the core damage and loop thermal-hydraulic aspects of the transient.

## REFERENCES


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2. C. A. Dobbe and R. Chambers, Analysis of a Station Blackout Transient for the Bellefonte Pressurized Water Reactor, EGG-NTP-6704, Idaho National Engineering Laboratory, October 1984.

3. Tennessee Valley Authority, Bellefonte Nuclear Plant, Final Safety Analysis Report, Docket 50-438, revised through Amendment 22, December 1982.

TABLE 1. COMPARISON OF COMPUTED AND DESIRED STEADY STATE PARAMETERS FOR BELLEFONTE PRA ANALYSES

| Parameter | RELAP5 | Desired |
|---|---|---|
| Core thermal power (MW)[a] | 3600. | 3600. |
| Pressurizer pressure (psia) | 2210. | 2210. |
| (MPa) | 15.2 | 15.2 |
| Pressurizer level (in) | 194. | 195. |
| (m) | 4.93 | 4.95 |
| Hot leg temperature ($^{o}F$) | 627.2 | 627.5 |
| (K) | 603.8 | 604.0 |
| Cold leg temperature ($^{o}F$) | 573.4 | 573.7 |
| (K) | 573.9 | 574.1 |
| Total loop flow (lbm/s) | 43722. | 43722. |
| (kg/s) | 19832. | 19832. |
| Reactor coolant pump head (psi) | 125.6 | 119. |
| (MPa) | 0.86 | 0.82 |
| Steam generator pressure (psia) | 1060. | 1060. |
| (MPa) | 7.31 | 7.31 |
| Steam generator liquid mass (lbm) | 31100. | 33660. |
| (kg) | 14107. | 15268. |
| Steam generator feedwater flow (lbm/s)[b] | 2279. | 2236. |
| (kg/s) | 1034. | 1014. |
| Feedwater temperature ($^{o}F$) | 477. | 477. |
| (K) | 520. | 520. |

[a] Core axial power shape based on 460 effective full power days.
[b] Per steam generator. Steam flow set to same value

TABLE 2. SEQUENCE OF EVENTS FOR BELLEFONTE FEEDWATER TRANSIENTS

| Event | Transient: 1.A | 1.B | 1.C |
|---|---|---|---|
| | Time (s) | | |
| Scram signal | 0.0 | 0.0 | 0.0 |
| Reactor coolant pump trip | 0.0 | 0.0 | 0.0 |
| Main feedwater valves begin to close | 0.0 | 0.0 | 0.0 |
| Turbine control valves begin to close | 0.1 | 0.1 | 0.1 |
| Turbine control valves closed | 0.2 | 0.2 | 0.2 |
| Main feedwater valves closed | 2.0 | 2.0 | 2.0 |
| Control rods fully inserted | 3.1 | 3.1 | 3.1 |
| Power operated relief valve initial opening | 230. | -- | 230. |
| HPI initiation | -- | 230. | 230. |
| Primary saturates (hot legs) | 1100. | 2200. | 2050. |
| Primary safety relief valves initial opening | 1200. | 330. | 2050. |
| Natural circulation ends | 1250. | 2300. | 2100. |
| Fuel cladding heatup begins | 1930. | -- | -- |
| Fuel cladding oxidation begins[1] | 2700. | -- | -- |
| HPI flow exceeds relief valve flow | -- | 5000. | 3500. |
| Transient terminated | 3236. | 11278. | 4696. |

[1]Potentially significant cladding oxidation assumed to begin when the hot spot cladding surface temperature reaches 1340 °F.

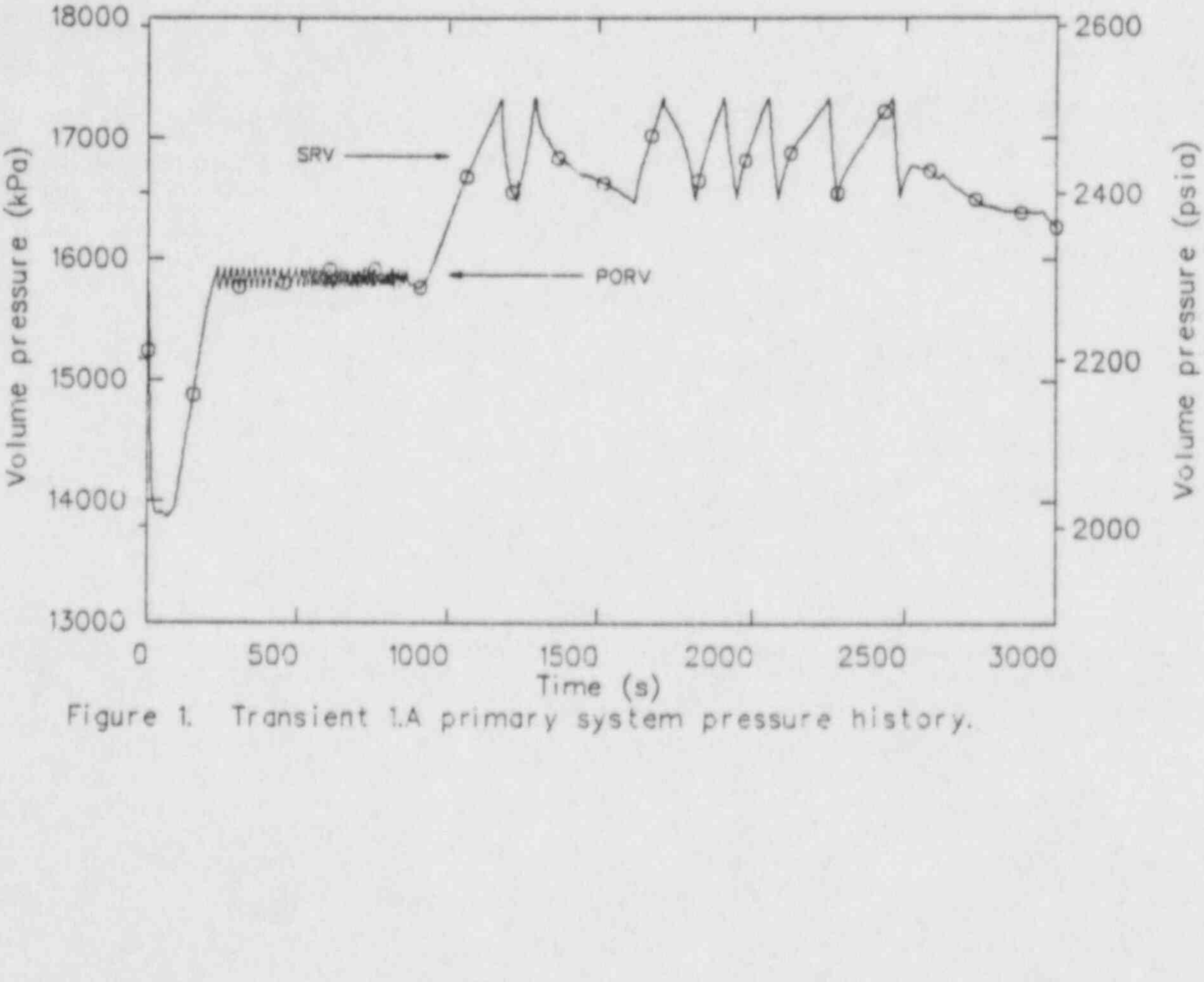

Figure 1. Transient 1.A primary system pressure history.

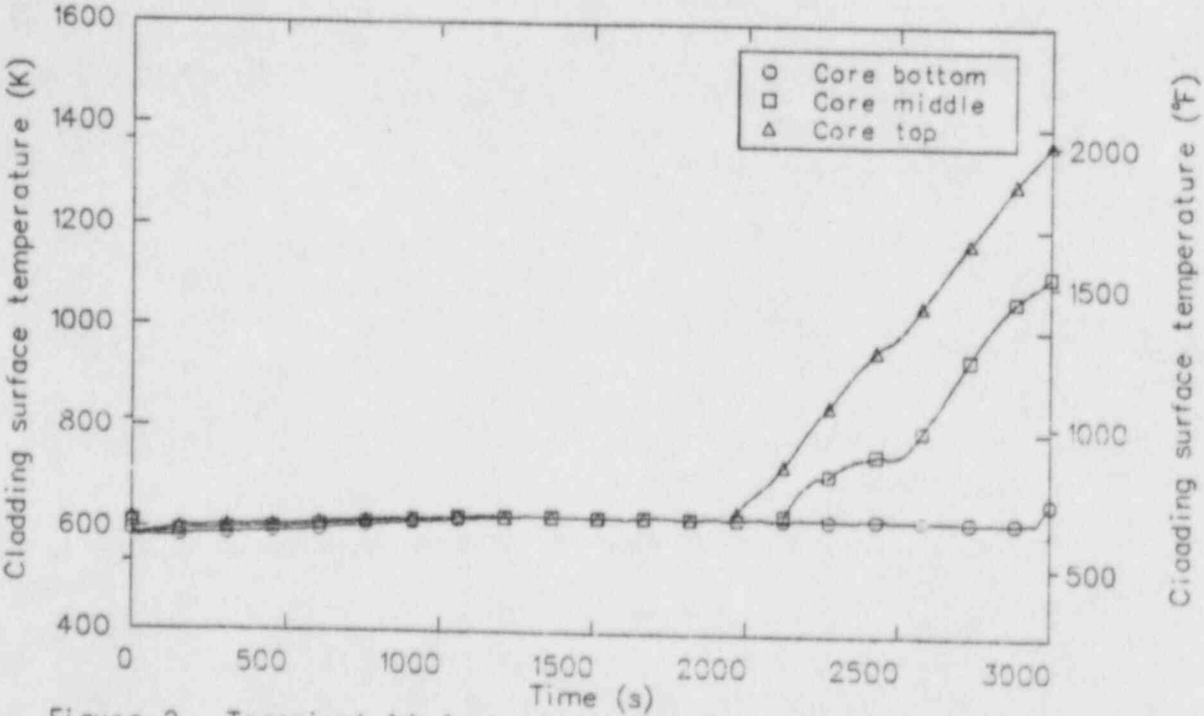

Figure 2. Transient 1.A fuel cladding surface temperature history.

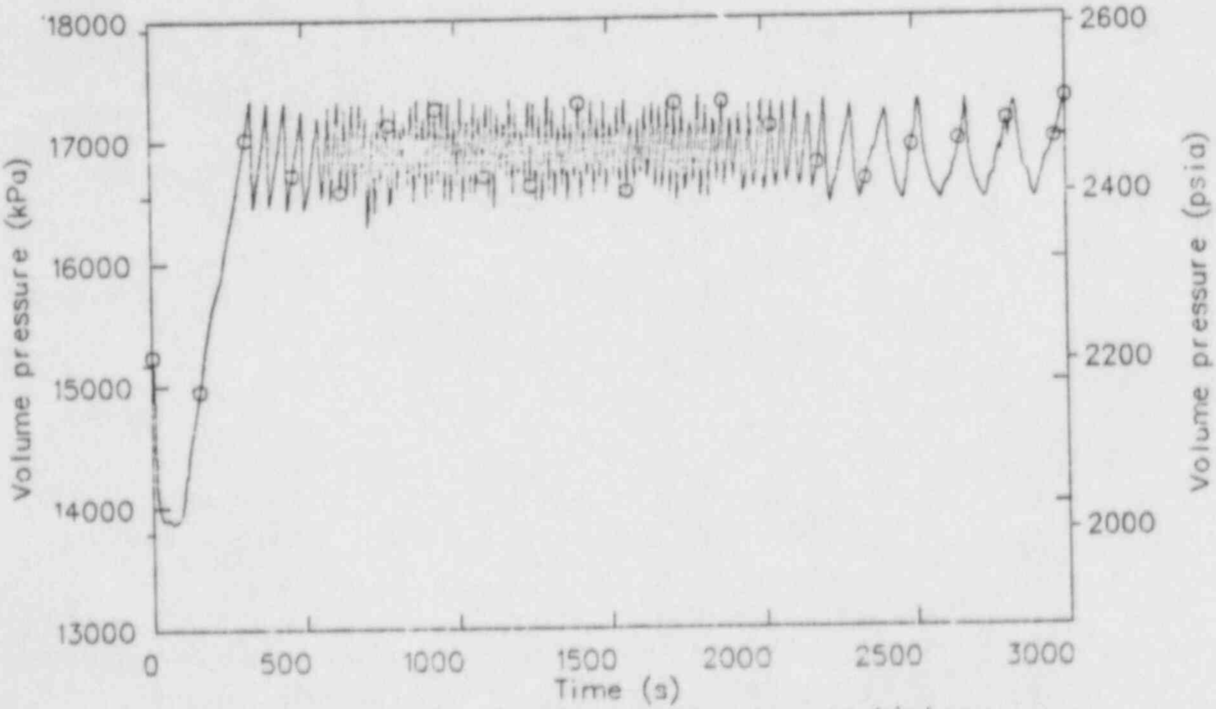


Figure 3. Transient 1.B primary system pressure history.

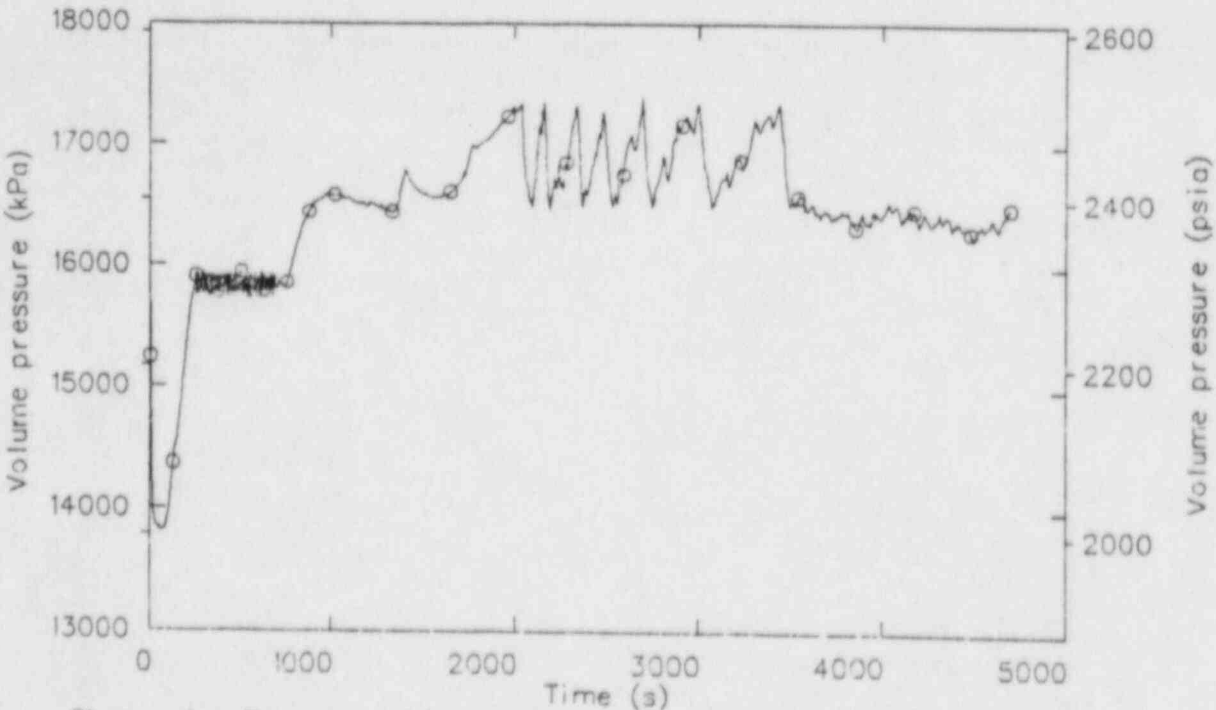

Figure 4. Transient 1.C primary system pressure history.

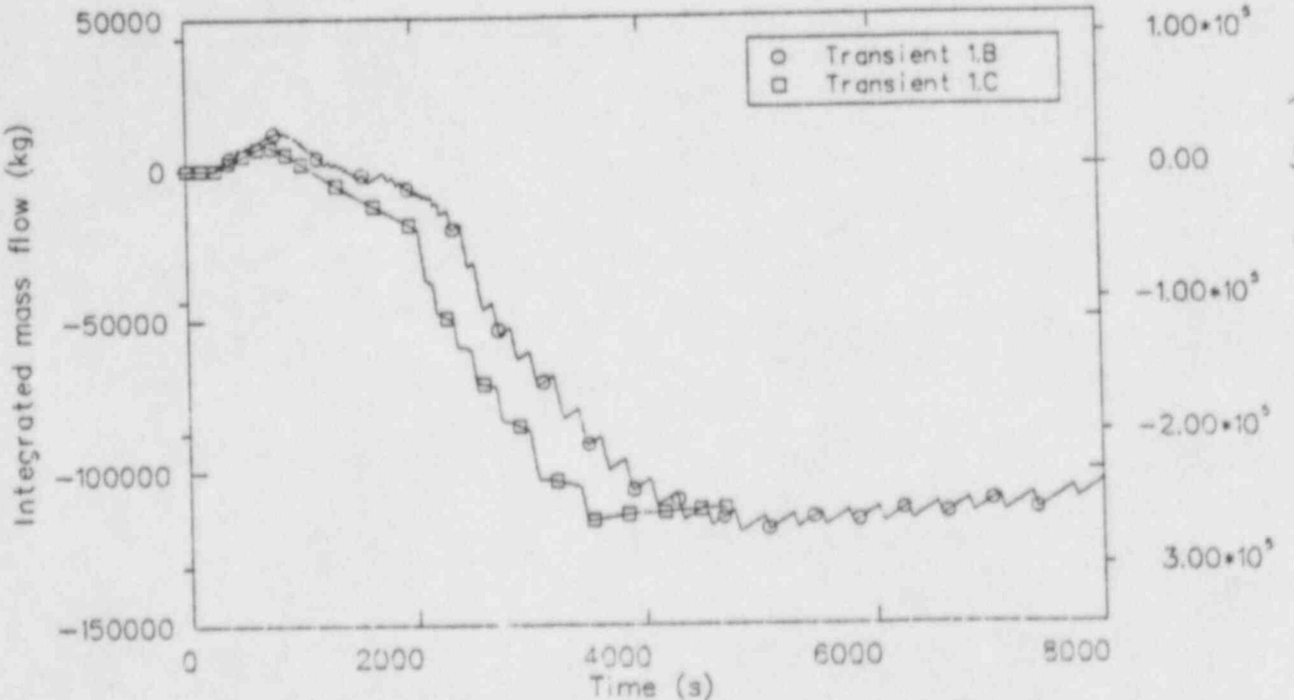


Figure 5. Primary system mass balance comparison for Transients 1.B and 1.C.

# THE UKAEA PWR SEVERE ACCIDENT CONTAINMENT STUDY

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## ABSTRACT

This paper describes the aims and some of the results of this Study, which began in the late Autumn of 1983 and has so far proceeded in two annual phases. The aims include the examination of conceivable events after representative hypothetical severe accidents and the assessment of computer models used for accident analyses. Three accidents have been studied, a large and a small break in the primary system and a transient. The work has been based upon a number of USNRC computer codes, such as SCDAP, MARCH, TRAPMELT and CONTAIN, supplemented by UKAEA codes in some areas. Some code development and a number of code inter-comparisons and analyses of experiments have been performed. The work covers in-vessel, cavity and containment events. The Study has shown, in a number of areas, that it is important to use mechanistic codes with integrated models. Only when these codes are firmly established and validated will it be possible to establish the worth of the more approximate, faster-running codes.

The work has been part funded by the Central Electricity Generating Board under the Thermal Reactor Agreement with the UKAEA.

## 1. INTRODUCTION

The UKAEA PWR Severe Accident Containment Study began in the late Autumn of 1983 as part of an Agreement between the UKAEA and the USNRC. So far it has proceeded in two annual phases and phase 2 is now nearing completion. A number of personnel from the UKAEA have contributed to the Study, and staff from the Central Electricity Generating Board (CEGB) and the National Nuclear Cooperation (NNC) have advised where appropriate. This paper describes the aims and overall content of the Study and discusses some of the results and conclusions.

The two main aims of the Study were defined as follows.

(a) To examine the conceivable events after representative hypothetical severe accidents in a large, modern PWR design, particularly those which might lead to a threat to the containment or a source of radionuclides to the environment.

(b) To assess the models used in the computer codes available for the analysis of such events, and to consider both the inadequacies of these models and identify areas where improvements are required. Experimental data have been used in such assessments where possible.

In Phase 1 these aims were addressed by studying both a large (AB hot leg, using WASH-1400 terminology) and a small break ($S_2D$) in a large modern PWR; a pressurised transient (TMLB') has also been studied in Phase 2. In order to concentrate on threats to the containment no containment by-pass accidents have been studied.

In AB hot leg a double ended guillotine break was taken to occur in one of the four hot legs of the primary circuit. Most of the primary circuit coolant is then expelled by the blowdown, and because a loss of electric power to safety systems is assumed no pumped ECCS water and no containment safeguards are available. In this scenario the core melts fairly soon and the path from the core to the break is short.

In the case of $S_2D$ a small break (9.5 - 50 mm) was assumed to occur downstream from one of the four steam generators, with no blowdown, but again with no electric power to safety systems, except for the containment fan coolers (at least 2 out of 4) and sprays. In this scenario the primary system depressurises much more slowly, the core melts later because the coolant is lost more slowly and the path from the core to the break is longer. The operation of the containment safeguards also present a difference relative to AB hot leg.

In the TMLB' scenario the accident is initiated by a transient and is accompanied by a loss of power to the safety systems, including the containment safeguards. The primary circuit remains at full circuit pressure up to circuit failure, discharging coolant through the relief valves as it heats up. The core will melt later than in $S_2D$, but the path from the core to the break will not be very different in length. The fan coolers and sprays are in-operative and so cannot reduce the containment pressure as in $S_2D$.

These three accident scenarios provided a useful vehicle for the Study because of the range of primary system conditions and timescales and the differences in the operation of containment safeguards. The last two also figure fairly high on many risk-based severe accident lists produced in various PRA studies, which adds to the relevance of the work performed in the Study.

Clearly an examination of in-vessel events, including the mode of vessel failure, are an important part of any containment study, in order to establish the sources to the containment of steam, hydrogen and radionuclides. This is reflected in the discussion in the paper. Cavity events are then discussed, including debris dispersal from the cavity, and core-concrete interaction and aerosol production. All of these influence events in the main containment volume and any radionuclide release to the environment. A discussion then follows on the threat to the containment arising from gas pressurisation, and direct heating by dispersed debris and gas combustion. An examination of aspects of the behaviour of radionuclide vapours and aerosols in the main containment accompanies this, with particular emphasis on the interaction of thermal-hydraulics and aerosol behaviour.

The plant data used in the Study are based upon that available [1, 2, 3, 4] for large modern designs, but does not purport to represent any particular plant. It is therefore to be regarded as typical of large modern PWR designs. The reactor nominal thermal power is taken to be 3411 MW, with enriched $UO_2$ fuel clad in Zircaloy-4. The containment is a massive pre-stressed and reinforced concrete vessel internally lined with steel and with a free volume of $\sim 9 \times 10^4$ $m^3$.

The work has been based upon a number of USNRC computer codes, with UKAEA codes introduced in some areas, such as for material inventory, vessel bottom head failure and containment aerosol studies. A considerable amount of code development work has been performed during the study, especially for the MARCH code, which has had a central role in the work. A number of code-comparisons have also been completed, such as between various routines in MARCH and other more mechanistic codes for core degradation (SCDAP), core-concrete interaction (CORCON Mod 2) and containment thermal-hydraulics (CONTAIN). These aspects are briefly discussed here.

## 2. PRIMARY SYSTEM BLOWDOWN, COOLANT BOIL-OFF, CORE MELT AND COLLAPSE

This stage of the three scenarios has been examined with a UK version of MARCH 1.0 [5], MARCH 2.0 version 151 [6] and two versions of SCDAP (Mod Ø Ver 3 and Mod Ø Ver 18) [7]. The MARCH code models a wide variety of physical phenomena, not usually in a mechanistic manner. SCDAP on the other hand includes detailed mechanistic models for bundle heat-up and meltdown, but it does not model the full primary circuit and so cannot, for example, compute the circuit pressure during the accident. The SCDAP/RELAP5 link [8] under development at EG&G Idaho does provide a full primary circuit capability, however, but has not been available for this Study. Only those aspects of the Study relating to the release of steam, hydrogen, hot gases and fission products from the core will be examined here, as these pose potential threats to the containment.

The system blowdown only occurs in the large br[illegible] case and has been treated in MARCH using a mass and enthalpy blowdo[illegible] table taken from Reference [3]. It is not modelled by SCDAP.

The results of the MARCH and SCDAP calculations are compared in Table 1. In MARCH the core has been represented by 10 radial and 24 axial nodes with associated burn-up profiles. In SCDAP it has been represented by 3 concentric radial annuli with different power ratings. The decay heat

## TABLE 1

## COMPARISON OF MARCH AND SCDAP RESULTS FOR THE CORE MELT PERIOD

| Parameter | AB hot leg# | | | | $S_2D$# | | | | TMLB'# | |
|---|---|---|---|---|---|---|---|---|---|---|
| | MARCH 1-UK | SCDAP MOD Ø VER 3* | MARCH 2 | SCDAP MOD Ø VER 18≠ | MARCH 1-UK | SCDAP MOD Ø VER 3* | MARCH 2 | SCDAP MOD Ø VER 18≠ | MARCH 2 | SCDAP MOD Ø VER 18≠ |
| Core Uncovery Time (secs) | 865 | 836 | 840 | 800 | 2746 | 2775 | 2058 | 2058 (by definition) | 8940 | 8940 (by definition) |
| Clad rupture time (secs) | - | Ballooning unreliable | - | 1400-1550 | - | 3600 | - | 2900-3000 | - | 10440 |
| Time significant oxidation begins (secs) | 1600 | 1300 - 1600 | 1700 | 1700 | 3600 | 3500 - 3900 | 3500 | 3300 | 10750 | 10440 |
| Time fuel and clad melt commences (secs) | 1800 | 2600 - 2700 | 2340 | 1950 | 3900 | 4600 - 4700 | 3660 | 3500 | 11220 | 11140 |
| Core collapse time (secs) | 2875 | Not modelled | 516[illegible] | Not modelled | 4890 | Not modelled | 6192 | Not modelled | 13758 | Not modelled |
| $H_2$ released up to MARCH core collapse (kg) | 302 | 136 | 389 (144 kg by 2300 secs) | 166 by 2300 secs | 327 | 164 | 475 (309 kg by 4300 secs) | 270 by 4300 secs | 589 (343 kg by 13200 secs) | 125 by 12140 secs |

*Using MARCH 1 decay heat (ANS 1971 standard) and pressure histories.
#Using MARCH 2 decay heat (ANS 1979 standard) and pressure histories.
#Core assumed to collapse when 75% molten in AB and $S_2D$. Slump assumed to start when 30% molten and finish when 75% molten in TMLB'.

and pressure histories for the SCDAP Ver 3 runs have been taken from the MARCH1-UK results, whilst those for the SCDAP Ver 18 runs have been taken from the MARCH 2 results. For AB hot leg the MARCH and SCDAP calculations have been compared from the point when the accumulator is assumed to fill the vessel up to the hot leg break. For the $S_2D$ calculations the comparison begins after the coolant level drops below the break for the MARCH1-UK and SCDAP Ver 3 calculations, but at core uncovery for the MARCH 2 and SCDAP Ver 18 calculations. The latter also applies to the TMLB' calculations.

In the AB hot leg case the core uncovery times predicted by the various versions of the two codes are very similar, as might be expected, since the codes are performing simple boil-off calculations up to that point. In all cases the MARCH 2 results predict a longer time to core collapse than MARCH1-UK, largely due to the inclusion of a model for radiation to the core barrel. The core heat-up rate predicted by MARCH1-UK is significantly higher than that predicted by SCDAP Ver 3, but MARCH 2 and SCDAP Ver 18 are in much closer agrement as shown in Figure 1 for the $S_2D$ scenario. The reasons for this change are difficult to define, but the MARCH heat-up rate appears to have been decreased mainly as a result of radiation to the core barrel and the SCDAP heat-up rate has been increased mainly because of improved thermal-hydraulic modelling at the boiling boundary and, in the later stages, less material is allowed to slump as nodes become molten, so maintaining the decay heat in the core. The SCDAP and MARCH results differ more significantly once melt relocation becomes important, MARCH having no mechanistic models in this area.

In most cases SCDAP predicts less hydrogen release by Zircaloy oxidation than MARCH, this is coupled with lower core exit gas temperatures. Investigations of this with SCDAP have indicated the importance of a number of models in addition to the Zircaloy oxidation rate constants used. Firstly, the inclusion of a ballooning model in SCDAP reduces the core exit temperature by ~200K in AB hot leg, by affecting heat transfer from the pins. Secondly, the use of a different core coolant voidage model in SCDAP reduces core exit temperatures by ~200K in AB hot leg, because it leads to less steam for Zircaloy oxidation. Thirdly, the inclusion of a Zircaloy oxidation hydrogen blanketing model in SCDAP Ver 3 reduces core exit temperatures by ~100K in AB hot leg, because of reduced Zircaloy oxidation. Hydrogen blanketing is not modelled in Ver 18, however, none having been detected in the latest oxidation experiments [9].

The above results show that particular mechanistic models, such as the inclusion of a model for radiation to the core barrel, can have a significant effect on phenomena of importance in the consideration of threats to the containment and in that respect lend support to further model development. One area of modelling not treated in SCDAP or MARCH is the possibility of 3-dimensional flow patterns in the primary circuit. These could have a significant effect on core heat-up rates and therefore on hydrogen, fission product and hot gas release to the contaiment.

An examination of the sensitivity of the SCDAP results to items of input data has highlighted the importance of the assumed axial power profile.

Sinusoidal profiles give peak pin temperatures ~200K hotter than the flatter profiles typical of the end of an equilibrium fuel cycle, with greater clad oxidation.

Versions of SCDAP have been compared in the UK with the PBT-ST and 1-1 tests and with the ACRR DF-1 test. The gross features of the temperature excursion that occurred in the Scoping Test (ST) were well predicted by SCDAP. Some confidence can therefore be placed in the predictions of the oxidation model under the steam rich conditions of that experiment. The analysis of PBF 1-1 shows that the SCDAP predictions are in good qualitative agreement also with that experiment, which became steam starved during the transient. The recent UK analysis of DF-1 also shows good agreement with the temperature excursion in that test. In all cases, however, the SCDAP calculations have under-predicted the hydrogen released, though in PBF-ST this was largely due to the inability of SCDAP to model the oxidation of the Zircaloy shroud used with the test bundle. Analyses of later PBF tests have been performed at EG&G Idaho and these also underpredict the hydrogen release. A comparison has been made between MARCH 2 and the results of the PBF-ST test [10]. The indications are that this version adequately predicts the initial heat-up phase prior to gross core distortion, but not the melting and slumping phase.

Whilst the code validation exercises are still underway and the code models are still being improved, the indications are that the SCDAP results reported here can be supported by experiment to a greater extent than those for MARCH. The hydrogen release and the core exit gas temperatures calculated by SCDAP are likely to be under-estimated, however.

## 3. MATERIAL INVENTORY AND RELEASE FROM THE CORE

The core inventory calculations used in the study have been calculated for the end of a 3 batch equilibrium fuel cycle, with a maximum fuel burn-up of 33,000 MWd/tU, actinide and fission product inventories being calculated with the UK code FISPIN [11] and its associated data libraries. As part of this work some comparisons with ORIGEN-2 [12] have been possible for the LOFT FP-2 experiment. This showed significant differences (10-14%) in the calculated masses of I, Sb, Sn and Ag, but not for the noble gases or other volatile fission products. Significant differences (10-50%) in the calculated decay powers at zero cooling time were also found for Sn, Ru, Ag, Nd, Sm and Rh. All arise from differences in the associated data libraries, for example the I mass difference arises from a difference in the I129 chain yield.

The inventories and the MARCH core temperature histories have been used to predict material release from the core by three methods. Firstly, using release rate coefficients [13] based on experimental data measured for irradiated fuel or fuel simulants and embodied in the UK codes MATREL [14] and FISREL [15]. Secondly, using gas-phase mass transfer mechanistic calculations for structural and absorber release, as embodied in the UK code FAEREL [16]. Thirdly, using FASTGRASS-VFP [17] for noble gas and volatile release. The results are given in Table 2 and Figure 2.

## TABLE 2

## PERCENTAGES OF MATERIAL RELEASED FROM THE CORE BEFORE CORE COLLAPSE

| Material | AB hot leg | | | $S_2D$ | | TMLB' | | |
|---|---|---|---|---|---|---|---|---|
| | MATREL | FAEREL | FASTGRASS | MATREL | FAEREL | MATREL | FAEREL | FASTGRASS |
| Cs | 89% | - | 74% | 74% | - | 95% | - | 86% |
| I | 88 | - | 78 | 75 | - | 95 | - | 89 |
| Xe | 89 | - | 84 | 75 | - | 95 | - | 90 |
| Kr | 89 | - | 84 | 75 | - | 95 | - | 90 |
| Te | 8 | - | 3 | 5 | - | 57 | - | 24 |
| Ag(f.p.) | 77 | - | - | 65 | - | 91 | - | - |
| Sb | 39 | - | - | 31 | - | 66 | - | - |
| Ba | 1 | - | - | 1 | - | 3 | - | - |
| Sn(f.p.) | 50 | - | - | 40 | - | 76 | - | - |
| Sn(Zry) | 50 | - | - | 40 | - | 76 | - | - |
| Ru | 0.2 | - | - | 0.1 | - | 0.4 | - | - |
| $UO_2$ | 0.1 | - | - | 0.1 | - | 0.3 | - | - |
| Zr(Zry) | 0.01 | - | - | 0.01 | - | 0.03 | - | - |
| Zr(f.p.) | 0.01 | - | - | 0.01 | - | 0.03 | - | - |
| Fe/Cr/Ni (Struct) | 1 | 30 | - | 0.8 | 1.1 | 3 | 2 | - |
| Mo | 3 | - | - | 2 | - | 9 | - | - |
| Sr | 1 | - | - | 0.8 | - | 3 | - | - |
| Ag(c.rod) | 59 | 100 | - | 52 | 14 | 64 | 3 | - |
| Cd(c.rod) | 77 | 100 | - | 67 | 100 | 82 | 100 | - |
| In(c.rod) | 39 | 100 | - | 35 | 27 | 44 | 15 | - |

The MATREL and FISREL codes, which represent the MARCH temperature history in different degrees of detail and interpolate between the temperature tabulated release rate coefficients differently, give very similar results. The release rate coefficients given in Reference [13] give less release than those in Reference [18] for some of the involatiles, such as Ba. This reflects a different evaluation of the available experimental data. The Te release given in Table 2 takes account of Te hold-up by reaction with the Zircaloy clad, the extent of this depending on clad oxidation, Zr preferring to react with oxygen rather than Te. This effect reduces the release of Te before core collapse in AB hot leg from 43% to 8%.

The results given in Table 2 do not cover release from the hot core once it falls into the bottom head. Release here is likely to be at a lower rate because the melt will probably form a single pool with a lower surface area, and there will be overlying water, at least for part of the time. If however the MATREL procedures are extended to the bottom head it is found that all the Cs, I, Xe and Kr are released in all three scenarios before bottom head failure, and the Te released is much increased.

The FAEREL results treat only gas-phase mass transport effects and examine whether they might impose a constraint on the MATREL releases, which do not allow for them. It is quite clear that these effects do not present a constraint for the structural or absorber materials in AB hot leg, but that they do present constraints for some of these materials in the higher pressure $S_2D$ and TMLB' scenarios, in which the gas flows are lower. In all the FAEREL cases 100% of the cadmium was released, based upon the cadmium vapour pressure. This is consistent with recent experiments conducted at Winfrith [19], where cadmium was shown to be the main component of the absorber rod aerosol. The MATREL results are based upon experiments performed at ORNL.

It is clear that the Xe, Kr, Cs and I releases calculated by the mechanistic code FASTGRASS-VFP are slightly lower than those of MATREL for this high burn-up fuel, but the tellurium release is a factor of 3 lower. It should be noted the FASTGRASS-VFP models Te hold-up by unoxidised Zircaloy. This level of agreement between MATREL and FASTGRASS-VFP for the noble gases and volatiles is encouraging because FASTGRASS-VFP has given fairly good predictions of the releases measured in the PBF and ORNL experiments. This gives some reason to suppose that MATREL gives high (pessimistic) releases.

The material releases given in Table 2 result in a material density of $\sim 3000\ g/m^3$ in the gases exiting the core, neglecting the noble gases. The likely chemical forms of these released materials is an important factor in determining whether vapours and/or aerosols are formed and where in the circuit vapours condense to form aerosols.

## 4. THE THERMAL-HYDRAULICS OF CORE EXIT FLOW AND PRIMARY SYSTEM MATERIAL RETENTION

This area is important because it influences the radionuclide source and gas temperatures entering the containment via the primary system break or the PORV in the TMLB' scenario. In Phase 1 of the study calculations have been performed with the MERGE [20] and TRAPMELT2 [21] codes, obtained from the USNRC but in Phase 2 a new UK code called SPRITE [22] has been used together with a UK version of TRAPMELT2 [22].

MERGE and SPRITE can calculate the gas and structure temperatures in the primary system from the core exit to the break, using MARCH core exit conditions. Both assume one-dimensional plug flow through the circuit and are used by dividing the circuit into a sequence of control volumes. SPRITE was developed from MERGE by improving some existing models, e.g. the correlations for convective heat transfer and the model for radiative heat transfer, by adding new models, e.g. for heat conduction in structures, and by changing the numerical procedure to a fully implicit scheme. Its accuracy has also been tested by using it to calculate the static part of the warm-up period prior to the fissium and corium injection in the Marviken tests [23]. It was concluded that SPRITE predicted the experimental temperatures within the spread of the measured data for a control volume and so could predict gas and wall temperatures through the circuit to ± 10%, provided the bulk flow could be treated as one-dimensional.

TRAPMELT2 can calculate the primary system transport and retention of CsI, CsOH, Te and a lumped aerosol. It has been used to analyse the aerosol injection period of the Marviken experiments. This work is still underway and so the conclusions are preliminary, but it appears that, with the enhancements made in the UK version of the code, the wall aerosol deposition arising from thermophoresis can be predicted quite closely, but that aerosol deposition in horizontal pipes may be overpredicted by a factor of 2 - 3, although this may be due to an incorrect calculation of the aerosol source to the pipes. In the Marviken pipework the major aerosol deposition mechanism is by gravitational settling. These conclusions arise from the first three Marviken tests, which did not use the full circuit including the reactor vessel and included fissium but not corium. Material was also injected at a lower temperature than in the remainder of the tests, so that these tests were aerosol transport tests and included very few vapour affects. The later tests include vapour and aerosol nucleation effects and emphasise the importance of including chemical speciation and aerosol nucleation models in TRAPMELT. These models are already planned for VICTORIA [24].

In Phase 1 MERGE and TRAPMELT2 calculations were performed for AB hot leg and $S_2D$. Very little material retention was calculated for AB hot leg, largely because the path to the break was short. The largest retention was for Te, where 20% of that released was retained by chemisorption on the structures of the upper plenum. $S_2D$ was more interesting in that the calculated material retention was greater, ~ 70% for CsI and CsOH, 90% for Te and 80% for the lumped aerosol. The Te retention was again mainly be chemisorption throughout the circuit, but the CsI and CsOH retention was largely by condensation on depositing aerosols in the steam generator. Condensation was not possible earlier in the circuit because of the hot gas temperatures, but became possible in the steam generator because of the strong cooling effect of the secondary side water, which was assumed to be an infinite heat sink. The aerosol deposition was large because of the long flow path and increased residence time, which leads to more time for deposition and also to increased agglomeration and thence more settling.

The use of SPRITE and the UK version of TRAPMELT2 has not changed these conclusions very much. The temperature profile in the upper plenum is flattened by the improved radiation treatment in SPRITE and the temperatures in the pipework are reduced by the inclusion of a heat conduction model, but these changes do not much affect the material retention, which remains small in AB hot leg and remains concentrated in the steam generator in $S_2D$. The results of the Phase 2 calculations for TMLB' are not yet available.

The work performed in the Study has indicated the difficulties in modelling primary system radionuclide retention and has highlighted areas where modelling improvements and further experimental data are required before adequate calculations can be performed. These are discussed in Reference [25].

## 5. DEBRIS PROGRESSION INTO THE LOWER HEAD AND VESSEL FAILURE

The progression of debris into the lower head, its interaction there with structures and water, and its mode of attack on the vessel have a large impact on the later ex-vessel part of the accident sequence. The quantities, composition and temperature of the debris that exits the vessel would determine the possible pressure spike in the containment, and thus whether or not an early containment failure was possible. Together with the mode of vessel failure they will also determine the possible dispersion modes of the debris and influence the likelihood of achieving long-term coolability.

The original MARCH code [5] has a parametric model for debris slump from the core into the lower head; the core may be assumed to collapse when debris slump first occurs, or at a later time (always prior to modelling attack on the vessel) depending on user input. MARCH 1 allows the user to choose a single particle model for clad oxidation in the lower head, and performs simple thermal equilibration calculations for the interaction of debris with two 'grid-plate' structures and the water in the lower head. Following the boil-off of water in the lower head and failure of the grid-plates, a more detailed model is used for thermal attack on the vessel, although in MARCH 1 rather arbitrary limits were placed on heat transfer from the debris to the vessel. Vessel failure is assumed to occur when its strength is sufficiently reduced, because of increased temperatures, to be unable to resist the internal pressure and static loading from the debris.

Work in Phase 1 of the Study concentrated on assessing the MARCH 1 phenomenology. During this assessment a number of corrections were made to the original code, including a complete re-coding of the grid-plate interaction models, and incorporation in MARCH1-UK. These corrections did not extend the modelling available in the MARCH code.

Consideration of material release from the core indicated that the lower fuel nozzles were the final impediment to release into the lower head. Scoping calculations suggested that the holes in the nozzles would plug independent of the assumed debris composition (Ag/In, Zr or a $UO_2/ZrO_2$ mixture). It was found that the larger holes in the lower core plate (first grid-plate modelled by MARCH) would not plug, but would be ablated by the flow of molten debris. Two sets of scoping calculations were performed: in the first it was assumed melt from half the core passed through one hole (below the first nozzle to fail); in the second it was assumed that, for each fuel assembly, debris passed through the corresponding holes in the lower core plate. In the first case final hole diameters in the range 0.23m to 0.82m were calculated as input assumptions were varied; in the second case the range for each hole was 0.06m to 0.11m. It was concluded that the lower core plate did not impede the debris progression, and that only a small fraction of it would be ablated in the initial interaction with the debris. It was unlikely to fail at this stage. The more massive lower core support plate has larger holes than the lower core plate and was therefore considered not to interact significantly with the initial pour of debris.

An assessment was made whether or not the pour of debris might itself fail the vessel as had been suggested in the Zion Probabilistic Safety Study [26]. It was not possible to eliminate this as a vessel failure mechanism, but with a debris temperature of 3123 K, a mass corresponding to half the core, a melt diameter of 0.3m and a fall velocity of $5m\ s^{-1}$ it was concluded that although 6mm of the vessel wall would melt locally, the temperature at 30mm into the vessel wall would only increase by 308 K. Thus it was concluded that failure of the vessel or failure of the penetration retaining weld would not occur using these input data. When consideration is also given to the effects of water breaking up the debris, and possibly protecting the vessel surface, it was concluded that and possibly protecting the vessel surface, it was concluded that this early vessel failure mechanism is unlikely.

On the basis of these assessments MARCH1-UK calculations were performed with the option of no interactions with the grid-plate, and without implementing a model for immediate vessel failure. It is recognised that the clad oxidation in the lower head calculated by MARCH depends strongly on the user's choice of particle diameters. For the Phase 1 calculations a particle diameter of 60mm was used and this gave about a 7% additional oxidation of clad in the lower head. The MARCH 1 assumption of thermal equilibration with the water always produces debris that is solid when attack on the vessel commences.

For the AB calculations the MARCH vessel failure model was used unaltered. Attack on the vessel was calculated to last for 75 minutes, compared with only 48 minutes to core slump after the start of the accident. The debris was molten at vessel failure; about half the vessel wall had melted prior to failure. For the $S_2D$ calculation a simple penetration retaining weld failure model was incorporated into MARCH1-UK. In this case the vessel was calculated to fail after only 12 minutes of the interaction without any melting of the vessel and with the debris still solid. This result apparently conflicted with earlier scoping calculations that indicated that the debris must re-melt before vessel failure [27]. An examination of the MARCH calculation showed that a considerably higher value of the debris thermal conductivity had been used; this high value might be achievable if molten metallic phases in the debris migrate to the boundary.

The MARCH models for attack on the vessel head by molten debris have been compared with the more detailed UKAEA code MELTPV [27]. The MARCH modelling of the thermal response of the vessel is judged to be adequate and that of heat transfer from the melt inadequate, but probably without significant consequences. The MELTPV single layer pool model has now been replaced by a two layer model in which the metallic melt is assumed to lie above the oxidic melt. The heat flux to the vessel wall is calculated to be around a factor of 3 higher from the metallic melt; however the likely vessel failure mechanism in pressurised sequences is still thought to be failure of one or more of the instrument guide- tube retaining welds, but at the outside rather than the centre of the guide-tube cluster.

Recently calculations have been performed with MARCH 2 Version 151 [6]. This allows a wider range of models of events in the lower head with one additional restriction - the intended, and preferred, MARCH 1 option of no interaction with the grid-plates has been removed. The use of finite heat transfer coefficients between debris and water in the lower head is welcomed, but as implemented, does not appear to make much difference to the accident sequence (the same energy is removed, but over a longer time). The extra sophistication of the heat transfer modelling has, however, been at the expense of consistency - hydrogen generation is still calculated using a single particle model which is not compatible with either the debris bed or flat plate heat transfer models. A further drawback of MARCH 2 is that it still forces collapse of the core before attack on the vessel begins - removing the versatility necessary to address some of the questions posed at the beginning of this section.

Many of the scoping calculations summarised in this section are described more fully in Reference [28].

The possibility of steam explosions in the lower head leading to vessel failure and possible threats to the containment has been reviewed for the Study. It was concluded that although understanding of steam explosion phenomena has increased significantly in recent years, uncertainty remains as to the likelihood and consequences of a large steam explosion in a PWR. In particular the difficulties in (i) understanding the core degradation processes and (ii) scaling from small- scale experiments were highlighted. The scoping calculations for the lower core plate described above indicated that it would take a minimum of a few seconds to get significant quantities of molten core into the lower head; this in itself may preclude large coherent steam explosions.

## 6. INTERACTIONS IN THE CAVITY AND WITH THE CONCRETE BASEMAT

In the AB sequence considered in the Study the cavity is dry at vessel failure, and there is no additional water available. Thus this sequence proceeds directly to core-concrete interactions. In the high pressure sequences considered ($S_2D$ and TMLB') the accumulators do not (according to MARCH) discharge before vessel failure. Thus as the primary circuit is relieved at vessel failure, it is expected that the accumulator water would be discharged via the vessel into the cavity. The blowdown of the vessel in high pressure sequences is considered likely to lead to sweep out of debris from the cavity region. The codes available to us have no treatment of debris dispersion, and no modelling of the dynamic effects of possible accumulator water addition.

Quasi-static models of the interaction of debris with water in the cavity are available in the MARCH code. In Phase 1 of the Study the $S_2D$ base case was defined to have no interaction with the water (an arbitrary decision, not based on physical reasoning); however, the MARCH 1 code does force interaction with the water for a short time. For the 3mm particle size used, this minimum interaction with the water had the effect of increasing the zirconium oxidation from 40% to 75.5%. On switching to the debris-concrete interaction module, INTER, heat transfer to the remaining water is modelled by a flat plate boiling correlation, and oxidation at the debris-water interface is no longer modelled.

The other MARCH 1 options for interaction of debris with the water have now been used for the $S_2D$ sequence, together with variations in the assumed particle size. The most surprising result of these calculations was that if debris was assumed to be coolable in the MARCH calculations, 3mm diameter particles would remain coolable in the cavity throughout, whilst 30mm diameter particles eventually dryout (at 301 mins) and attack on the basemat is then predicted. This result is explained by the observation that the higher steaming rate from the smaller particles leads to a higher containment pressure and the triggering of the sprays; this does not occur for the larger particles, which nevertheless evaporate all the available water in the cavity.

The MARCH 2.0 code has been used to model the interactions between debris and water for the TMLB' sequence. A 3mm particle size was chosen, and a debris bed model selected. The results of the calculation were, however, dominated by the MARCH 2 'levitation' model that invokes an isolated particle model for the heat transfer. In the calculation the lower head failed at 246 mins, and the debris (initially at 2150°C) is quenched within 15 secs. The remaining accumulator water is boiled off by decay heat at 448 mins, and the debris then heats up to 1813 K at 532 minutes when the debris-concrete interaction model is invoked.

So far only a cursory study of these events in the cavity, prior to possible debris-concrete interactions has been possible. The provision of a variety of models for the interaction in MARCH is welcomed, given the present state of knowledge, but the assumption that debris must be in the cavity is considered to be too restrictive.

In the AB hot-leg sequence attack on the concrete in the cavity commences immediately after vessel failure; the cavity is dry and there is no further addition of water. Whether or not attack on the concrete occurs for the $S_2D$ sequence depends on the assumptions that are made about debris coolability. Depending on these assumptions, the cavity may contain water at the start of the interaction, or be dry (the water having been boiled off before the start of the attack on the concrete). In the absence of containment heat removal, attack on the concrete is inevitable in the TMLB' sequence; as for the $S_2D$ sequence the cavity may be wet or dry at the start of the interaction. In all the calculations that have been performed it has been assumed that the debris is confined to the reactor cavity and the instrument tube tunnel (these have a total floor area of 59.5 square metres). It is recognised that, in the high pressure sequences, debris may be dispersed over a larger area, and that outside the cavity it may have a greater access to the water than is assumed in the calculations.

Three codes have been used to investigate the interaction of core debris with concrete. These are Murfin's INTER code [29], as included in the MARCH 1 [5] and MARCH 2 [6] codes, a substantially revised version of INTER (INTER UK) [30], and, recently, the CORCON-MOD2 code [31]. Early experience with the INTER code led to the following conclusions: (i) that some of the important model assumptions had been superceded by experimental evidence (e.g. the mechanism, and temperature of concrete decomposition - which is assumed to be caused by chemical decomposition of the cement in INTER [29], but appears to arise only at the higher melting point of the aggregate in experiments [32]; (ii) that areas of the code suffered from lack of detailed verification (e.g. radiation from the metallic layer, when it overlay the oxide layer, was erroneously set to zero); (iii) that, at that time, there was no other code available with the flexibility of INTER (e.g. CORCON-MOD1 [33] did not have a model for overlying water); and (iv) that a code of about the complexity of INTER was satisfactory for scoping containment failure issues. Thus, in general this initial assessment supported the statement on INTER in the Sandia assessment of MARCH [34], whilst indicating areas needing improvement.

Corrections and improvements to the INTER coding have been made on an adhoc basis, resulting in the current version of INTER UK, which was used in Phase 1 of the Study. Although INTER UK uses much of Murfin's original coding, the changes that have been made mean that the modified code gives very different answers from the original. In INTER UK the debris is assumed to consist of at most two layers, which are well-mixed and contain oxidic and metallic phases respectively. The cavity is always assumed to consist of a cylinder with a spherical cap base, and so as the interaction progresses approximations are necessary to maintain this calculational shape. Heat transfer from the bulk of a debris layer to its boundary is given by heuristic correlations developed by Murfin [29] - the heat transfer coefficient depends on the state of the layer (solid or liquid) and on the magnitude of the gas blowing rate. A fixed heat transfer coefficient is used for the gas film that is assumed to form between the debris and the concrete (the original INTER model has been discarded). The concrete is assumed to decompose at 1600 K (significantly higher than in the MARCH versions of INTER). The rate of ablation of the concrete is calculated by allowing for the heat flux conducted into the concrete, rather than using a heat of ablation that includes the sensible heat requirement. The code assumes that the temperature profile ahead of the melt-front is a decaying exponential; a weighted residual method has been used to derive an equation for the evolution of the thermal penetration distance, and this part of INTER has been entirely recoded. Release of chemically bound water and carbon dioxide from the concrete is assumed to be in proportion to the mass of concrete ablated; however the unbound water is assumed to be released at its boiling point, and the mass released is calculated using the assumed temperature profile. Examination of the thermal penetration model indicated that the concrete specific heat should be chosen to be consistent with internally calculated enthalpies for the concrete components. INTER UK uses the lumping of chromium with zirconium, and nickel with iron, employed in the original code for the chemical interactions. Changes have been made to force preferential oxidation of zirconium, and make the heats of reaction consistent. INTER UK does allow for heat transfer to an overlying pool, but a consistent treatment of the boiling curve and radiation has only recently been incorporated - too late for the sets of calculations performed in the Study. A further error that has been found recently, and is present in all published versions of the code, affects the heat balance at the start of the interaction; this error causes excessive ablation at this stage (by up to a factor of about 2).

In the calculations the basemat is assumed to be basaltic concrete (composition based on CORCON default composition [33]) with 11 percent by mass rebar steel. For the AB case INTER UK calculations start 142 minutes after the beginning of the accident sequence. At this time the debris consists of 101 te of $UO_2$, 12.8 te of Zr, 10.45 te of $ZrO_2$ (clad 38 per cent oxidised) and 38.1 te of steel. The debris temperature is 3029 K and both metallic and oxidic phases are molten. This high initial temperature causes rapid penetration in the first hour of the interaction (0.48 m vertically, 0.60 m horizontally), during which both debris layers fall to below 2000 K. In this time the remaining Zr and Cr have been almost completely oxidised, and the layers have inverted to place the oxide on the top. After 2 hours of the interaction the rate of penetration is much reduced and is only driven by the decay heat generated in the debris. For the remainder of the calculation (up to 48 hours) the oxide temperature remains at about 1800 K (oxide molten), whilst the metal falls to its freezing point (1618 K), and varies between being solid and liquid (partial solidification, which is expected in practice, is not allowed by the code). After 10 hours of the interaction both the horizontal and vertical penetrations are close to 1 m. By 40 hours into the interaction the vertical penetration is 1.35 m (basemat still intact), the horizontal penetration is 1.60 m, 421 te of concrete have been ablated releasing 5.6 te of carbon dioxide (68 percent reduced to carbon monoxide) and 35.5 te of steam (40 percent reduced to hydrogen). A plot of the cumulative energy balances for this run is shown in Figure 3. After 40 hours the decay heat input totals 1.82 TJ, compared with 0.11 TJ input from chemical reactions. The major heat loss is by radiation - 1.28 TJ in total, whilst the total heat to the concrete is 1.08 TJ. This latter quantity of heat is mainly retained as the sensible heat of the molten concrete; only 0.16 TJ has been added to the atmosphere (taking the zero for enthalpy of the added materials at 273 K), and about 0.05 TJ has been stored in the solid concrete. The large contribution in radiation is particularly noteworthy as both MARCH 1 and MARCH UK ignore this as a source to the containment; in MARCH 2 it can be used to decompose further amounts of concrete.

As described above, the debris in the $S_2D$ calculation is solid at vessel failure. In the base case calculation (minimum interaction with cavity water) INTER UK calculations start immediately after vessel failure with 147 te of water lying above the debris. There is minimal erosion of the concrete in the first hour of the interaction - this is the time it takes for the oxidic debris to re-melt. Early calculations shows that the metal layer would solidify in this period, but new calculations with the improved model for heat transfer to water show the bulk of the metal is liquid, with a solid upper crust. Once the oxidic phase is molten erosion of the concrete proceeds at an increased rate (still slower than in the AB sequence - partly as a result of having less Zr to oxidise), so that after 4 hours of the interaction the penetration in both directions is only about 0.30 m. It takes 7 hours for the cavity to dryout (4 hours in the earlier calculations). The maximum temperature of the metal layer during the whole interaction is 1800 K. After the cavity has dried-out the attack on the concrete is very similar to that calculated for the AB sequence. Of the heat input in the first 40 hours of the interaction 1.03 TJ was radiated, 0.78 TJ was transferred to the concrete, and 0.32

TJ was used to boil water. INTER UK calculations have not been performed for TMLB'.

Some calculations have been performed with the version of INTER in MARCH2. This version is closer to the original code than is INTER UK, however some corrections have been made and new modelling introduced. A comparison run for the AB sequence gave 726 te of concrete ablated after 40 hours according to MARCH2, compared with 421 te for MARCH UK. This difference arises from the observation that MARCH2 requires 1640 kJ to ablate 1 kg of concrete, while MARCH UK requires 2560 kJ. It is believed that calculations with the versions of INTER in MARCH1 and MARCH2 are likely to be conservative, because of this low energy of ablation.

CORCON-MOD2 provides much more detailed models for the evolution of the pool shape, the chemical interactions, and the heat transfer mechanisms. Initial assessment is very favourable, although it is recognised that many of the models are unvalidated in detail, and may require modification to obtain agreement with experiments. The use of chemical enthalpy is especially welcomed, and provides a model for other codes involving a number of different species with heat transfer and chemical reactions. The assessment, so far, has turned up one modelling deficiency; namely that the use of a specific heat between the solidus and liquidus that contains a portion of the latent heat has unexpected influences on the heat transfer modelling, resulting in unphysical restrictions on crust growth.

A CORCON-MOD2 calculation for the first 5 hours of the AB sequence has been compared with the INTER UK calculation discussed above. The vertical penetration rates are similar, but the radial penetration rate and mass of concrete ablated have been reduced by 40 percent. In this period, CORCON-MOD2 reduced 82 percent of the steam evolved to hydrogen compared with the INTER UK value of 56 percent. The CORCON calculations show an absence of CO in the period of 0.5 to 1 hour after the start of the interaction (due to reduction of $CO_2$ to carbon), followed by an increased release of CO in the next hour (as the carbon is re-oxidised following completion of the oxidation of zirconium). The temperature histories of the oxide predicted by the two codes are very similar, but the temperature of the metal is higher and closer to the oxide temperature than was found in the INTER UK calculation. In both calculations radiation is the dominant heat loss mechanism in the first 5 hours. The larger mass ablated by INTER may be due to the error in the heat balance at early times (when the pool is mainly core debris); the energy required to ablate 1 kg of concrete in the CORCON calculation (2306 kJ) is slightly lower than the INTER UK value.

In the future it is expected that CORCON-MOD2 integrated into the MARCH2 code will be used, probably using MARCON2.OP [35] as a basis for this development. Consideration of the $S_2D$ sequence, though, highlights the need for further development, as the debris is unlikely to settle into the separated layers required by the core-concrete interaction codes until after it has re-melted, rather than before, as is assumed in current calculations.

Much experimental work on aerosol production during core-concrete reactions has been performed at Sandia since 1975 [36] and more recently work has been done at KfK. The Sandia work has shown that this aerosol source is an important potential contributor to the source term. This Study has highlighted the significant uncertainties associated with estimates of the magnitude and content of this aerosol source. The KfK work has also underlined the uncertainties associated with this work, in that they have seen [37] much less aerosol than has been seen at Sandia, although this may be due to the type of concrete used and work is underway to explore this possibility.

The experimental work has not yet been fully reported, but it is clear that the aerosols are produced by four mechanisms, three of which are associated with the gas bubbles or columns rising through the melt from the core/concrete interaction. These mechanisms are (a) mechanical aerosol formation on bubble bursting; (b) entrainment of melt in the gas flow and subsequent break-up due to hydrodynamic forces; (c) condensation of vapours released in bubbles; (d) condensation of vapours released from the melt surface.

The experients completed so far have employed melts of 12 - 200 kg and yet the results are employed in accident scenarios involving $10^5$ kg of melt. This is a large scaling factor, which could change the relative importance of some of these mechanisms. This requires investigation.

In this study the Murfin and Powers correlation [38] has been used to calculate the aerosol source, together with a Sandia execution [39] of the VANESA [40] code for AB hot leg. The latter showed that an estimated $10^3$ kg of aerosol is generated between 3.6 hours and 5.9 hours, which is to be compared with the earlier release of $2 \times 10^3$ kg of aerosol from the hot leg break during the core melt-down. Comparisons of this VANESA prediction with the early Murfin and Powers correlation show the latter to be a factor of 5 - 10 lower. This difference is not unreasonable considering that the correlation was based upon only a few early experiments.

The VANESA code represents the only current attempt to produce a mechanistic model for core-concrete aerosol production. It is therefore making an important contribution to modelling a potentially important aerosol source. It is clear from the published information on VANESA, however, that it does not model some important aspects of the experimental data such as the following: (i) the experiments indicate a particle size distribution which is at least bimodal, but VANESA omits the large size mode, which would settle most rapidly; (ii) VANESA omits mechanism (b) above, which from the experimental data appears to be very significant during the early phases of the interaction.

It is also clear [41] that aspects of the existing models result in significant uncertainty. For example VANESA assumes that the melt consists of an oxide layer overlying a metal layer, whereas it is thought that the metal layer will be on top early in the core/concrete interaction, when the temperatues will be highest and the release rate greatest. The exact situation effects the chemistry in the rising bubbles and can significantly affect the material released as aerosol;

the release of the lanthanides is especially sensitive to this and is lowest when the metal layer is on top.

One might expect the very short-term and long-term (if present) core-concrete aerosol sources to be most important. The very short-term because of the vigorous initial core-concrete interaction and the very long-term because all other aerosols would have settled by then. These two phases of the core-concrete interaction are the least understood at present, indeed it is not clear whether any aerosol is produced in the very long-term.

## 7. CONTAINMENT THERMAL-HYDRAULICS, GAS COMBUSTION AND PRESSURISATION

Here we consider the containment thermal-hydraulics and compare the predictions of three lumped-parameter codes employed in Phase 1 of the Study, namely a UK version of MARCH 1.0 [5], CONTAIN [42] and SARACEN [43] for AB hot-leg and $S_2D$. MARCH1-UK contains corrections to obtain improved mass and energy balances. The major changes include the use of internal energy instead of enthalpy to determine containment atmosphere conditions, the relaxation of the restriction to the saturation line so as to allow for the possible presence of superheated steam, and the correct calculation of the mass of water condensed on the permanent heat sinks. SARACEN is a thermal-hydraulics code being developed in the UK which is similar to MARCH except that a full set of steam tables is used to cover all possible steam conditions and more detailed modelling of condensation, evaporation and sensible heat and mass transfer is provided. In Phase 2 of the study MARCH2 Version 151 [6] was also used and some results are given here.

Some results for AB hotleg are presented in Figures 4 and 5. The different pressures predicted by CONTAIN and SARACEN derive partly from their being passively driven by MARCH1-UK, so that any feedback effects which can be modelled in MARCH cannot be treated, but more importantly from different steam tables and the improved detailed thermal-hydraulic modelling enshrined within them, such as for wall condensation heat transfer.

A particularly noteworthy feature of the MARCH2 AB hot leg results is that the hydrogen in the containment never becomes flammable, due to steam inerting, while MARCH1-UK predicts that the hydrogen is flammable for a substantial period and will give a containment pressure of 10.8 bar (absolute) for an instantaneous adiabatic burn.

Containment codes, such as MARCH and CONTAIN permit the user to simulate an actual hydrogen burn within a compartment and between compartments by determining whether the atmospheric mixture is flammable, and if so a burning velocity, which may depend on the containment atmosphere composition, is computed and used to calculate the resultant compartment pressure and temperature rise. Because the finite burning time permits heat to be lost to floors, walls and active heat sinks (eg fans and sprays), the final pressure rise is less than that predicted from the instantaneous adiabatic burn calculation.

However the true result of a burn of flammable gases probably lies between the two calculations. Two important factors have been neglected - geometry (including compartment shape and size and location of ignition point) and atmospheric turbulence. The former is important because the lower flammability limit for a hydrogen burn depends on the direction of burning - thus 4% (vol) is sufficient for vertically upwards while 8% (vol) is required for vertically downwards. Moreover, the flow patterns within the compartment may lead to significant spatial variation of concentration of flammable gas. Also, the magnitude of the burning velocity depends on direction as well as concentration, thus the location of the ignition source is important. The second neglected factor, atmospheric turbulence, also affects the burning velocity. At low levels, this is increased while at very high levels of turbulence the burning is damped down and the flame may be extinguished. Again, local compartment geometry plays a significant part in determining turbulence levels.

Neither geometry nor turbulence can be addressed by lumped-parameter codes like MARCH or CONTAIN. Thus two factors which are at least as important as the concentration of steam or hydrogen in calculating burning velocities are necessarily neglected. This implies that the use of finite burn times must be heavily qualified before it can be used with confidence in a given situation.

Should a PWR core melt down, fall into the bottom head and eventually cause the head to fail, then the containment may experience pressure rises due to interaction of the debris with any water in the reactor cavity, due to attack by the core on the concrete basemat leading to the evolution of steam and non-condensible gases, and due to possible hydrogen burns. Another means of increasing the containment pressure arises if the core is expelled from the reactor pressure vessel under high pressure (eg. through an instrument tube penetration in a TLMB' or $S_2D$ sequence), as it may then be possible for fine particulate core debris to heat the containment atmosphere directly.

Calculations have been performed to examine the effect of instantaneously mixing various masses of core debris at initial temperatures of 1500 K, 2250 K and 3000 K with the atmosphere of a large modern PWR dry containment of volume $8.83 \times 10^4$ $m^3$ initially at 393 K. For dry air and 70 te of core the maximum calculated pressure is 3.1 bar, while if the initial containment atmosphere includes saturated steam at 100% relative humidity the maximum pressure rises to 5.1 bar. Finally, if it is assumed that 7 te of unoxidised zirconium is released and intimately mixed with the 70 te of core and that oxidation occurs preferentially with air, releasing 12 MJ per kg zirconium reacted, then the maximum pressure is 7.1 bar, so that assuming a design pressure of 3.45 bar (gauge), with normal engineering safety factors in excess of 2, no containment failure is anticipated.

It is also instructive to look at the corresponding position within the reactor cavity, volume 297.6 $m^3$. For air only and 15 te of core the maximum pressure is 8.5 bar, while the addition of steam raises this to 21 bar. Because of the smaller mass of gas to be heated in the cavity compared to the containment significantly smaller core masses are required. This is highlighted by the results which include zirconium oxidation, when 1 te of core debris, plus 0.1 te of Zircaloy, initially at 3000 K, can raise the reactor cavity pressure to 19 bar.

The above calculations assume instantaneous heat transfer, ignoring the timescale over which heat transfer can occur. For a 1 mm radius particle a flight path of around 75 m is required for it to lose 95% of its heat by radiation and forced convection. In view of the changes in flowpath required to accommodate such a distance and the need to avoid sizeable structures in the cavity and elsewhere it is unlikely that such a particle will transfer all its heat to the atmosphere. Instead, it is much more likely to impact, and heat, a structure. For particles of 100μ radius and less, however, the picture is very different. There appears to be ample distance for them to transfer their heat to the atmosphere.

The SPIT and HIPS experiments [44] at Sandia indicate that up to 1% of the discharged core simulant is contained within particles of less than 10μ diameter and up to 10% within those of up to 100μ diameter. Since efficient heat transfer to the cavity or containment atmosphere can be performed by particles of up to 100μ diameter, it is suggested that up to about 10 te of core debris should be considered in studies of the direct heating effect if the whole core of a large modern PWR is assumed to be ejected.

It is concluded that should direct heating to the maximum containment free volume occur, the pressures are unlikely to pose a threat to the containment unless other events (eg, steam explosions or hydrogen burns) occur simultaneously. If the heating occurs within the reactor cavity then, unless there is an efficient vent to the main free volume, large pressure rises could occur, but it is not clear what effect these would have on the cavity.

## 8. THE BEHAVIOUR OF VAPOURS AND AEROSOLS IN THE CONTAINMENT

This topic is important in order to determine the loss of radionuclides to the environment via a containment leak or break. In the Containment Study it has been examined with the UK code AEROSIM [45] and the USNRC Sandia code CONTAIN [42]. AEROSIM is a stand-alone single component aerosol code originally developed for the study of sodium fire aerosols and recently extended to include the modelling of steam condensation effects, which it treats with a time-dependent condensation rate taken from a separate thermal-hydraulics code, such as the MACE module of MARCH. CONTAIN can treat the thermal-hydraulics and aerosols physics in an integrated manner and can therefore model feedback effects.

Before using the two codes for the accident scenarios examined in this Study an experiment [46] involving a dry uranium oxide aerosol, in an enclosure at constant temperature and pressure, was calculated. This provided a situation in which the thermal-hydraulics was not important and so a comparison of the aerosol models could be made. This case is known as ABØ1(ST) in Reference [46]. The work showed that at long times (72500 secs) the two codes gave very similar masses airborne and settled, but CONTAIN predicted faster aerosol settling at short times (~2000 secs). The latter effect was mainly due to the use of the Fuchs [47] collision efficiency for gravitational agglomeration, which is three times larger than the more exact Pruppacher-Klett [48] formulation. The use of the latter would reduce the early settling rate in this problem by about a factor of two. The Pruppacher-Klett formulation is preferred because it treats both particles involved in the collision as moving, whereas Fuchs assumes that one is stationary. This work has therefore suggested a straightforward but worthwhile improvement to the CONTAIN code; the improvement is already in AEROSIM.

A fairly detailed analysis of the containment aerosol behaviour for the AB hot leg scenario has been performed, whilst the analysis of the other two scenarios is less complete. Only the AB hot leg work will therefore be discussed here. In this scenario the first source to the containment is the blowdown coolant, some of which will form an aerosol. No experimental data to characterise such an aerosol source were available to the Study, and so a source was created for CONTAIN by seeding the atmosphere with many very fine particles and then allowing the blowdown steam to condense on these to give atmospheric steam saturation, creating 123 te of suspended water aerosol at 20 seconds. CONTAIN predicts that only 0.013 te of this aerosol is still airborne by 1765 seconds, when the significant part of the core meltdown release begins. AEROSIM predicts 1.6 te remains still suspended at this time, however. The main reason for the difference is that the hot gases (up to 1900 K) released from the primary circuit from 850 seconds onwards cause most of the water aerosol in CONTAIN to evaporate, a process which cannot be modelled by AEROSIM. This observation illustrates the importance of performing coupled thermal-hydraulic and aerosol physics calculations. It might be thought that the difference between 1.6 te and 0.013 te airborne is not important in comparison with the 123 te airborne originally. This is not so however, because when the 1.6 te of water aerosol remains airborne to assist deposition almost half of the 2 te core melt release is deposited by the time this release ceases, but only 6% of the core melt release is deposited during this period when only 0.013 te of water aerosol remains airborne. Clearly the water aerosol would be an efficient means of rapidly removing the core melt aerosol, were it not to largely deposit or evaporate beforehand.

The exact behaviour of the blowdown aerosol becomes relatively unimportant to the airborne aerosol mass by the time the 1.2 te core-concrete aerosol release begins at 8525 seconds, however. From that point onwards to 12 hours the CONTAIN and AEROSIM calculations are in closer agreement, although CONTAIN predicts water condensation onto the core-concrete aerosol, but AEROSIM does not, so that CONTAIN gives a faster deposition rate.

## 9. CONCLUSIONS

It is clear that the Containment Study has made progress in achieving its two broad aims, but a considerable amount of work remains to be done before the aims have been fully satisfied. The work will therefore continue with the overall intention of producing a calculational route that can be used with some confidence for a large modern PWR.

The Study has shown, in a number of areas, that it is important to use mechanistic codes with integrated models. The core meltdown area provides an example of this, with a number of individual phenomena all affecting the core temperatures, oxidation rate and core exit gas temperatures. Material release from the core provides another example as the core temperatures determine release and the gas-phase mass transfer constraint on release is dependent upon the thermal-hydraulics. Similarly the decay heat carried by the released radionuclides can affect the primary system thermal-hydraulics and hence material retention in the primary system. The containment thermal-hydraulic and aerosol modelling also provides another example, as important interactions can occur. These are examples of a general conclusion which suggests that integrated mechanistic computer codes are now required if our understanding and modelling of severe accidents is to progress further. This lends support to the development of codes like RELAP5/SCDAP/TRAPMELT [8], TRAC/MELPROG [49], CORCON, VANESA and CONTAIN. The use of a modelling boundary at vessel failure is probably adequate, as there is unlikely to be any significant feedback from the containment to the primary system up to that point. It is probable that CORCON and VANESA should be combined in order to model feedback effects, such as the loss of decay heat and heat of vaporisation [41] as the aerosols are released. Only when the integrated mechanistic codes are firmly established and validated will it be possible to establish the worth of the more approximate codes, such as MELCOR [50] or MARCH 2.

The main difficulties are in establishing whether the computer codes are treating all of the necessary phenomena, in ascertaining the accuracy of that treatment and in relating that accuracy to the requirements, as far as they are known. SAUNA [51], QUEST [52] and this Containment Study have attempted to tackle some aspects of the first two parts of this problem, but each is necessarily inadequate at a time when the importance of some phenomena have been very inadequately researched, e.g. gas circulatory effects in the primary system or material resuspension as a result of sudden and substantial containment failure.

Code comparisons with integral and separate effects experiments are required to establish whether the necessary phenomena are being modelled and the accuracy of those models. A large amount of experimental and associated code analysis work is underway, although more emphasis needs to be placed on the it in the next few years, even at the expense of plant calculations, and some gaps remain in the existing or proposed experimental programme, such as in the field of very long- term core-concrete interactions. Whilst effort should be found to deal with these and other similar issues it would be helpful if the existing knowledge could be applied to establishing more quantitative requirements for the computer models when used in plant predictions.

## 10. ACKNOWLEDGMENTS

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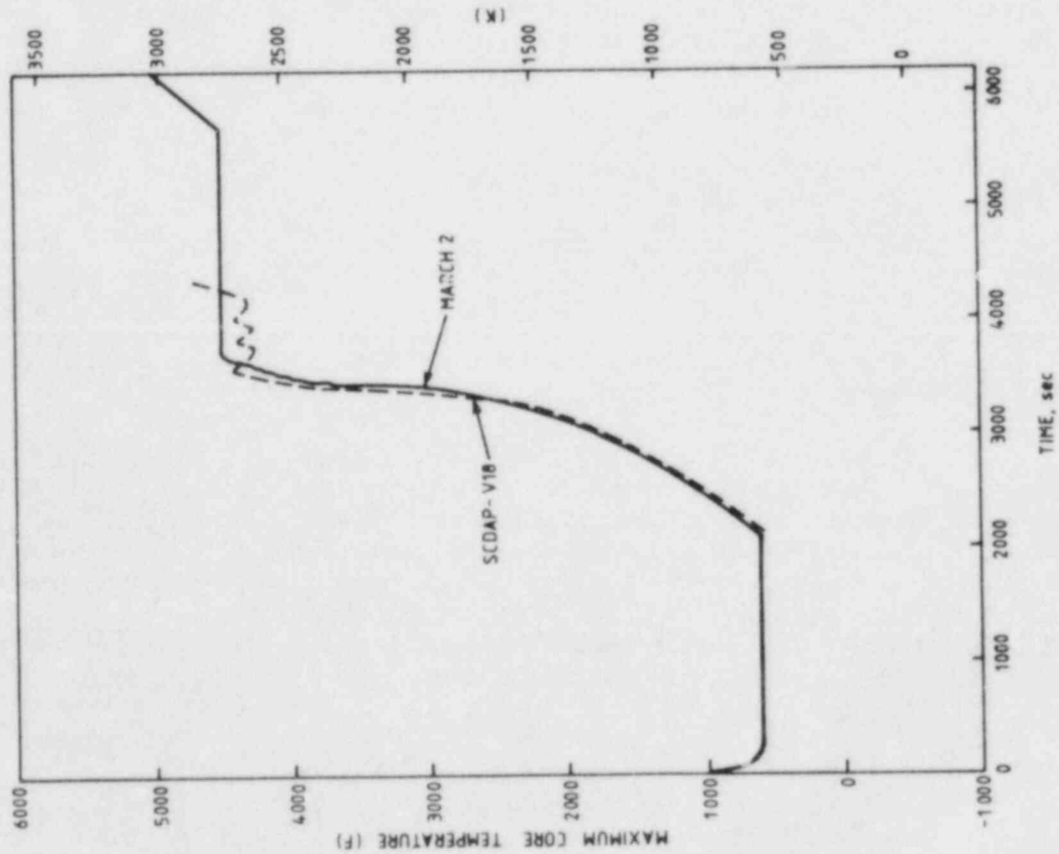


FIG 1 MAXIMUM CORE TEMPERATURE PREDICTED BY MARCH 2 AND SCDAP-V18 FOR $S_2D$

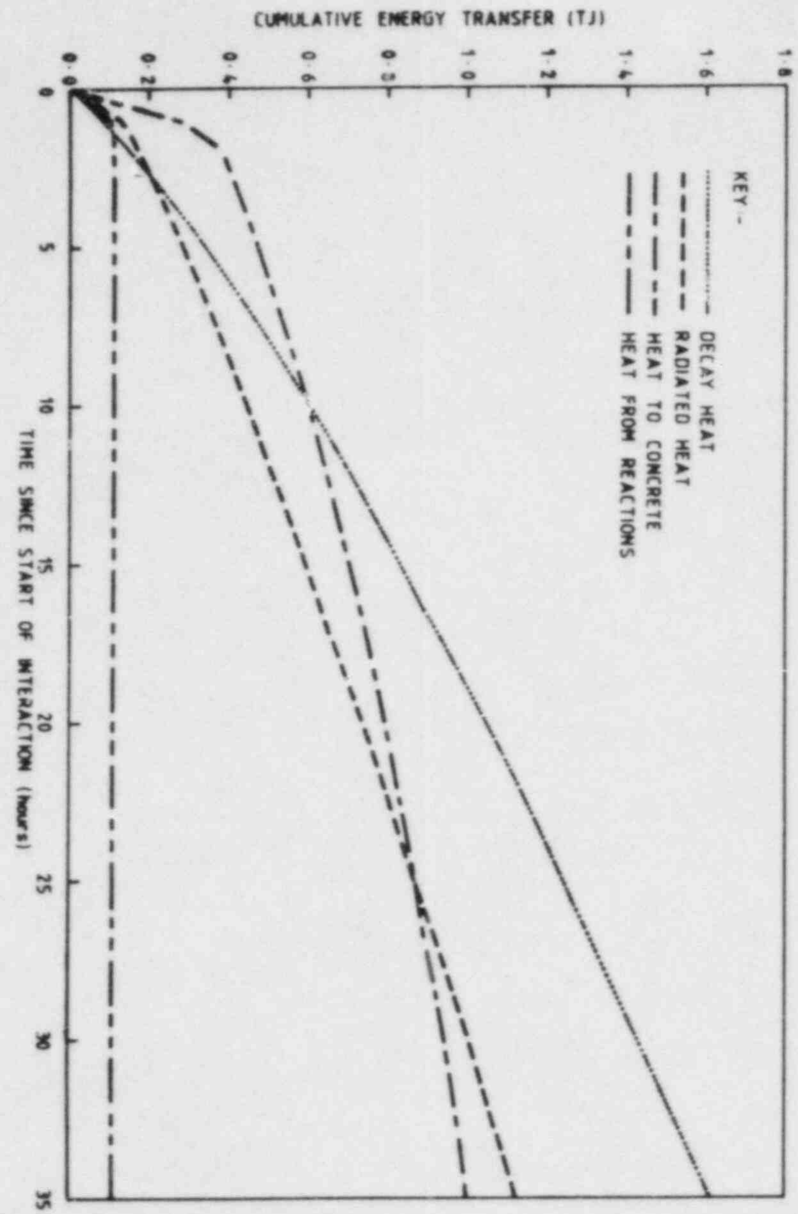


FIG.3 CUMULATIVE ENERGY BALANCE

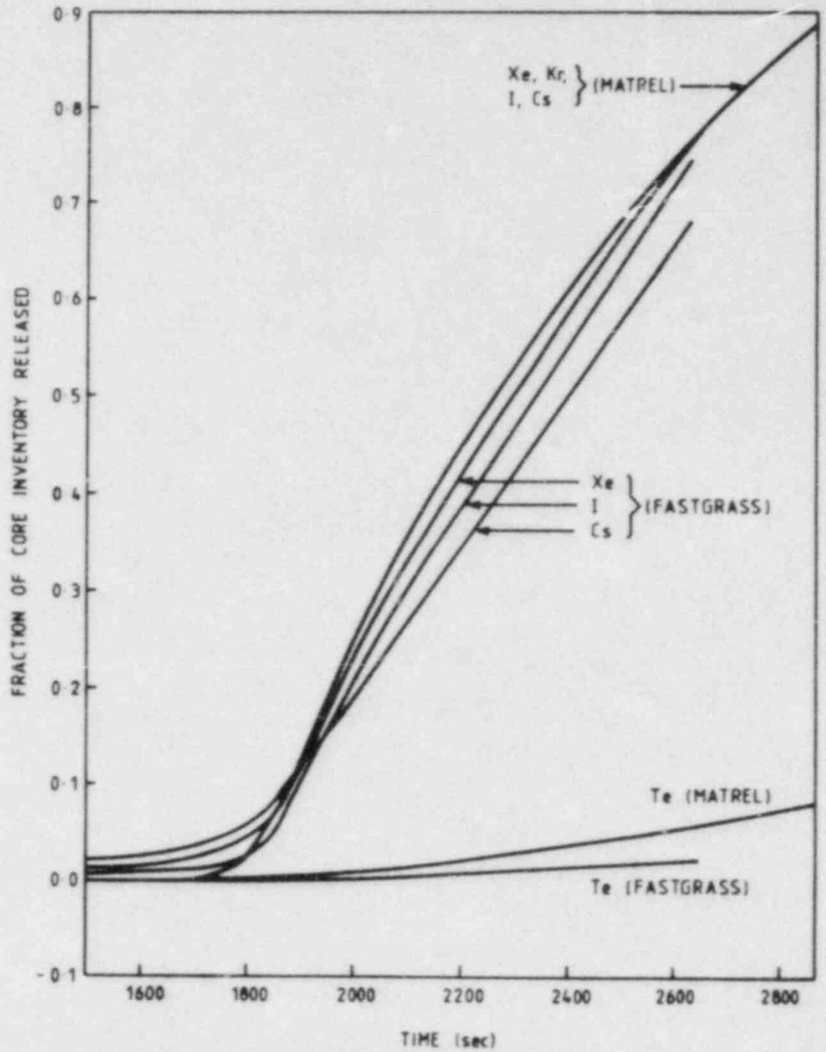


FIG 2 MATREL / FASTGRASS PREDICTIONS OF WHOLE - CORE RELEASE OF VOLATILE FISSION PRODUCTS FOR AB HOT LEG

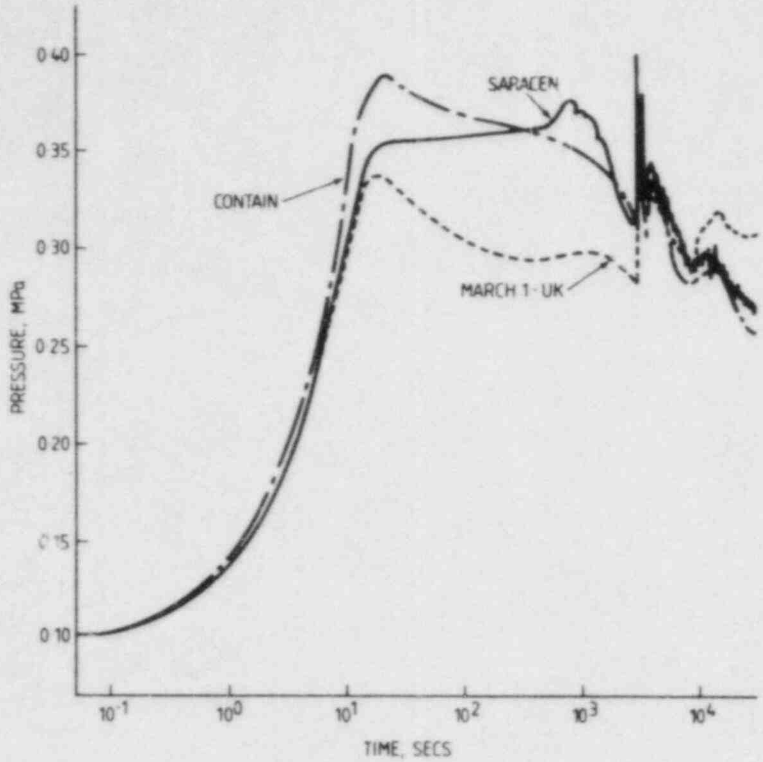


FIG. 4. MARCH - SARACEN - CONTAIN COMPARISON AT EARLY TIMES FOR AB HOT LEG

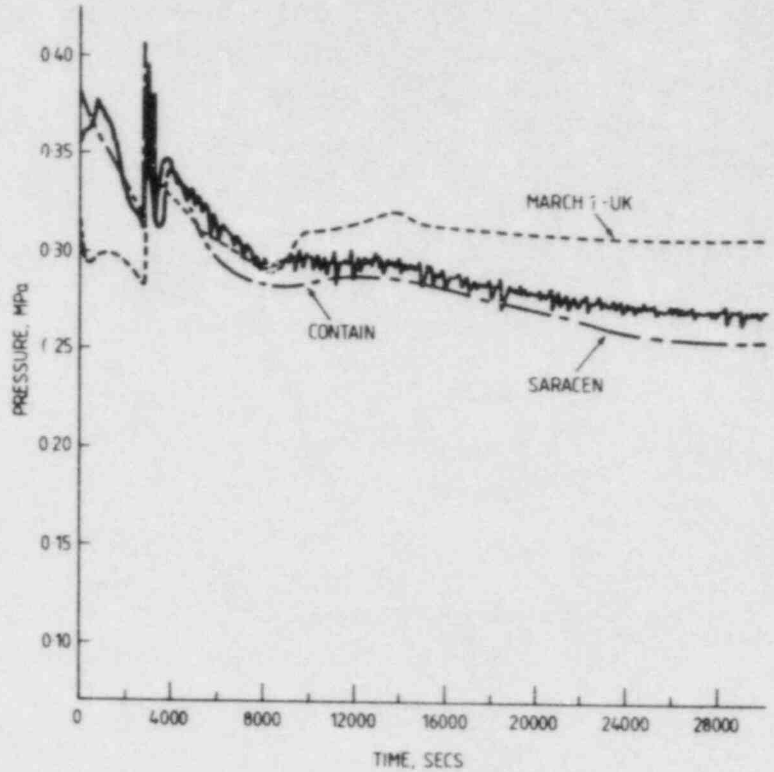


FIG. 5. MARCH - SARACEN - CONTAIN COMPARISON AT LATER TIMES FOR AB HOT LEG

# STATION BLACKOUT CALCULATIONS FOR BROWNS FERRY

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## Abstract

This paper presents the results of calculations performed with the ORNL SASA code suite for the Station Blackout Severe Accident Sequence at Browns Ferry. The accident is initiated by a loss of offsite power combined with failure of all onsite emergency diesel generators to start and load. The Station Blackout is assumed to persist beyond the point of battery exhaustion (at six hours) and without DC power, cooling water could no longer be injected into the reactor vessel. Calculations are continued through the period of core degradation and melting, reactor vessel failure, and the subsequent containment failure. An estimate of the magnitude and timing of the concomitant fission product releases is also provided.

## Background

The Oak Ridge National Laboratory (ORNL) has participated in the Severe Accident Sequence Analysis (SASA) program since it was established in 1980 by the Containment Systems Research Branch of the Nuclear Regulatory Commission. The SASA program at ORNL has examined potentially severe accidents at Boiling Water Reactors (BWRs), with the objective of establishing as realistically as possible the sequence of events and consequences of each accident. The Browns Ferry Unit 1 BWR has been utilized, with the full cooperation of the Tennessee Valley Authority, as the example plant for the accident studies.

The ORNL SASA program has performed detailed studies of five BWR accident sequences: Station Blackout (Ref. 1), Small Break LOCA Outside Primary Containment (Ref. 2), Loss of Decay Heat Removal (Ref. 3), Loss of Injection (Ref. 4), and Anticipated Transient without Scram (ATWS) (Ref. 5). An estimate of the magnitude and timing of fission product releases was published for Station Blackout (Ref. 6), Small Break LOCA Outside Primary Containment (Ref. 7) and Loss of Decay Heat Removal (Ref. 8).

Station Blackout (Refs. 1 and 6) was the first ORNL SASA study (completed in 1981). During the interviewing four years, significant improvements in analytical modeling capabilities (i.e., computer codes) have occurred. In light of these code improvements, the decision was made to repeat the Station Blackout calculations.

## ORNL SASA Code Suite

Determination and analysis of the events in an accident sequence that would occur prior to core uncovery is made by the simulation program BWR-LTAS (Ref. 9, developed at ORNL by R. M. Harrington). The basic assumption of the BWR-LTAS code is that the reactor vessel, internals, and fuel are undamaged. The thermal-hydraulic conditions are calculated for both reactor vessel and primary containment. The programming provides flexibility to model the effect of operator actions. The code simulates all interacting plant systems that determine accident sequence development. The predecessor of the current BWR-LTAS code was developed for the original Station Blackout calculations; but the code has been upgraded and expanded to meet the needs of each subsequent accident sequence studied at ORNL.

The original BWR-LTAS calculations (Ref. 1) for the Station Blackout study demonstrated the importance of depressurizing the reactor vessel before battery failure. The capability to calculate both reactor vessel and primary containment response over the period before core uncovery allowed investigators to fully understand that depressurizing the reactor vessel limits drywell temperature, reduces the total number of required SRV actuations, and extends the time to core uncovery after battery failure. The conservative approach, however, is to assume that the reactor vessel is not depressurized (i.e., the core would uncover at ~8 hours after scram instead of ~10 hours for the case with depressurization). The calculations for the sequence without depressurization are presented in this paper.

The response of the primary system and primary containment during the period of the accident sequence following core uncovery is determined by the MARCON 2.1B and MELRPI codes. MARCON 2.1B is based upon MARCH 2.0 (Ref. 10) but utilizes CORCON (Ref. 11) instead of INTER for the corium-concrete interactions and employs the ORNL SASA program BWR models. These include representation of all special BWR features such as channel boxes, control blades, and safety relief valves and incorporation of properties routines that are correct for the saturated conditions within a BWR vessel. The code also includes models for the reaction of the $B_4C$ control rod powder with steam. Additional major model changes in MARCON for both in-vessel and ex-vessel phases of the simulation are presented in Table 1. The original ORNL Station Blackout calculations (Ref. 1) used MARCH 1.1 (Ref. 12) for the period of the accident following core uncovery. The deficiencies in early versions of MARCH with regard to BWR modeling have been extensively identified (Refs. 2, 4, 13).

MARCON 2.1B represents a major step forward in user ability to model BWRs; however, additional calculations are still required from a detailed BWR core degradation code MELRPI (Refs. 14, 15). The core melt and melt relocation models in MARCON are simplified and non-mechanistic; so, the MELRPI code results provide guidance in selecting MARCON input which results in the 'proper' progression in MARCON calculations of the core melt relocation. The application of MELRPI to the Station Blackout calculations also resulted in new model changes in MARCON. This represents the first application of MELRPI in a SASA accident study. The use of MELRPI is discussed by Ahmet Sozer in another paper presented at this WRSR meeting entitled "MELRPI Development and Use."

The BWR secondary containment model (SCM) was developed and first used in the fission product transport analysis of the small break LOCA outside containment sequence (Refs. 2 and 7). The SCM was originally developed because the containment model provided in MARCH subroutine MACE does not permit analyses that includes adequate representation of the response of BWR secondary containment structures.

The purpose of the secondary containment model is to calculate the response of the reactor building to in-leakage from the drywell under accident conditions. The model also calculates the response of the refueling floor to in-leakage from the reactor building and in-leakage (or exfiltration) from the atmosphere. Other factors such as heat sinks, condensation of steam, fire protection sprays and the standby gas treatment system (SGTS) are included in the SCM. However, the SGTS would not be operational during Station Blackout.

The individual inventories of the six constituents that make up the atmospheres in the reactor building and refueling bay flow control volumes are determined at each time step; these are steam, $CO_2$, CO, $H_2$, $N_2$, and $O_2$. The leakage rates of each constituent from the drywell into the reactor building are taken directly from the MARCON code output, as is the temperature at the leakage source (reactor vessel or drywell).

The ORNL code TRENDS was used to estimate transport and retention characteristics for the following groups of volatile elements: xenon-krypton, iodine-bromine, cesium-rubidium, and tellurium-selenium. The TRENDS code analyzes fission product behavior in the primary coolant system as well as in both the primary and secondary containments, and includes models of the following processes:

1. releases from fuel (failed or intact)
2. convective transport in liquid or gas flows
3. chemical interactions
4. radioactive decay

Fission product inventories at shutdown are obtained from the ORIGEN2 code [16] and are calculated for each individual cell in the 5 × 5 core nodalization. Releases are determined from core fuel using a modification of the NUREG-0772 [17] model and from the drywell rubble by the VANESA code [18]. Convective transport is determined using thermal hydraulic information from MARCON and SCM, and the assumption that each control volume is instantly well-mixed. Aerosol behavior for the current analysis was done using the QUICK code [19], although current plans are to use a more comprehensive model in future work.

Chemical interactions in the reactor vessel include dissolution in water, as well as condensation (CsI, CsOH, $CsBO_2$) and adsorption (HI, I, $I_2$, $Te_2$) onto both steel surfaces and airborn aerosols (which may subsequently deposit). The species distribution is recalculated at each time step by the SOLGASMIX code [20], which solves for the equilibrium distribution by minimizing free energy. Currently included are 18 gas phase and 8 condensed species, including CsI, CsOH, HI, and $CsBO_2$. In the primary and secondary containment volumes, models are again included for deposition of various species onto surfaces and aerosols; dissolution in water pools is modelled using partition coefficients. Species redistribution currently involves the two iodine reactions

$$I^-(aq) + I_2(aq)$$

$$I_2(g) + CH_3I(g)$$

Another important characteristic of the TRENDS code is its inclusion of decay chain modelling. Decay equations that include all relevant parent-daughter relationships are solved numerically by the fourth order Adams predictor-corrector scheme. Decrement due to decay and increment due to precursor decay occurs for each nuclide, in each control volume, at each time step. Thus, precursor transport and contributions to daughter transport are automatically included.

## Station Blackout Sequence

This study describes the predicted response of Unit 1 at the Browns Ferry Nuclear Plant to a hypothetical Station Blackout. This accident would be initiated by a loss of offsite power concurrent with a failure of all eight of the onsite diesel-generators to start and load; the only remaining electrical power at this three-unit plant would be that derived from the station batteries. It is assumed that the Station Blackout occurs at a time when Unit 1 is operating at 100% power, and only Unit 1 is assumed to be affected. The 250 volt DC battery system at Browns Ferry could remain operational for a significant period of time. In response to AEC inquiry in 1971, during the period of plant construction, TVA estimated that the steam-driven High Pressure Coolant Injection (HPCI) and Reactor Core Isolation Cooling (RCIC) systems, which use DC power for turbine control and valve operation, could remain operational for a period of four to six hours. Subsequently, in 1981, TVA performed a battery capacity calculation which shows that the unit batteries can be expected to last as long as seven hours under blackout conditions. A period of six hours has been assumed for this study.

The initial ORNL Station Blackout study (Ref. 1) demonstrated that it would be beneficial for operator action to depressurize the reactor vessel early in the initial phase of a Station Blackout. This depressurization removes a great deal of steam and the associated stored energy from the reactor vessel at a time when the RCIC system is available to inject replacement water from the condensate storage tank and thereby maintain the reactor vessel level. Subsequently, when water injection capability is lost for any reason, remote-manual relief valve operation would be terminated and there would be no further water loss from the reactor vessel until the pressure has been restored to the setpoint [7.72 MPa (1105 psig)] for automatic relief valve actuation. Because of the large amount of water to be reheated and the reduced level of decay heat, this repressurization would require a significant period of time. In addition, the subsequent boiloff would begin from a very high vessel level because of the increase in the specific volume of the water as it is heated and repressurized. Thus, an early depressurization will provide a significant period of valuable additional time for preparative and possible corrective action before core uncovery after injection capability is lost. This study conservatively assumes that there is no depressurization.

## Results

### Thermal-hydraulic behavior of the primary and secondary containments:

Within 30 seconds following the inception of a Station Blackout, the reactor would have scrammed and the reactor vessel would be isolated behind the closed main steam isolation valves (MSIV's). The initial phase of the Station Blackout extends from the time of reactor vessel isolation until the time at which the 250 volt DC system fails due to battery exhaustion. During this period, the operator would maintain reactor vessel water level in the normal operating range (Fig. 1) by intermittent operation of the RCIC system, with the HPCI system available as a backup (Fig. 2). Each of these water-injection systems is normally aligned to pump water from the condensate storage tank into the reactor vessel via a feedwater line.

The Control Room instrumentation necessary to monitor reactor vessel level and pressure and for operation of the RCIC and HPCI systems would remain available during this period.

The operator would also take action during the initial phase to control reactor vessel pressure by means of remote-manual operation of the primary relief valves. The primary relief valves would actuate automatically to prevent vessel overpressurization if the operator did not act; the purpose of pressure control by remote-manual operation is to reduce the total number of valve actuations by means of an increased pressure reduction per valve operation and to permit the steam entering the pressure suppression pool to be passed by different relief valves in succession. This provides a more even spacial distribution of the transferred energy around the circumference of the pressure suppression pool.

The plant response during the initial phase of a Station Blackout can be summarized as an open cycle. Water would be pumped from the condensate storage tank into the reactor vessel by the RCIC system as necessary to maintain level in the normal operating range. The injected water would be heated by the reactor decay heat and subsequently passed to the pressure suppression pool as steam when the operator remote-manually opens the relief valves as necessary to maintain the desired reactor vessel pressure. Stable reactor vessel level and pressure control is maintained during this period, but the condensate storage tank is being depleted and both the level and temperature of the pressure suppression pool are increasing. However, without question, the limiting factor for continued removal of decay heat and the prevention of core uncovery is the availability of DC power.

The station batteries fail after six hours operation; subsequently the operator can no longer manually actuate the SRVs or inject water into the vessel. Thus begins a monotonic decrease (boiloff) in the reactor vessel water level (Figs. 1 and 3) due to intermittent loss of fluid (steam) through the primary relief valves which actuate automatically.

Without restoration of power, the operator can do nothing to impede the progression of the accident. The core uncovers at 479 minutes and the core structures begin heating up, oxidizing and melting. Significant core structural relocation (molten control blades and canisters) begins at 572 mins, this downward relocation immediately increases steaming which decreases water level (Fig. 3) and increases SRV actuation (Fig. 4) until core plate dryout occurs (at 630 mins) at which time steaming ceases. The pressure (Fig. 4) decreases after core plate dryout due to leakage through the MSIVs to the condenser. Fuel melting starts at 604 mins and structural relocation continues (dropping onto a dry core plate) until the core plate fails (on temperature) at 682 mins. By user input, core plate failure occurs when the combined core plate and debris are at 964 K (1275°F); at this time, there is approximately 33500 kg (73853 lbs) of solidified debris resting on the core plate. The core, however, is supported by the control blade guide tubes in the bottom head; and, although the core plate and debris fall into the bottom head and are quenched, the core remains in place until collapse (50% molten) at 695 mins. [The 50% criteria was used since it produced close agreement between MARCON and MELRPI.]

After core collapse, the core debris boils off the water in the vessel bottom head over a period of ~15 mins and, in the process, the core debris cools to 1580 K (2384°F). The debris then reheats, eventually failing a bottom head penetration at 734 mins. This causes the vessel to depressurize (Fig. 4) until the vessel pressure equalizes with the drywell pressure (Fig. 5). At this point, the corium is still solid; it is assumed to leave the vessel when it reaches a liquid state at 2200 K (3500°F).

The liquid corium leaves the vessel at 797 mins, falling into two 1900 liter (500 gal) sumps. After vessel failure, the containment pressure increases due to boiling of the water initially in the drywell sumps. The containment fails shortly after the corium/concrete reaction starts (~805 mins). For the remainder of the accident, the drywell thermal/hydraulics are dictated by the corium/concrete reaction with very high drywell atmospheric temperatures being predicted (Fig. 6).

A synopsis of the major events in the accident and the event timing is presented in Table 2. These events are clearly reflected in the in-vessel water level (Fig. 3) and pressure (Fig. 4) responses and in the drywell pressure response (Fig. 5). It should be noted that there is a significant period of time between reactor scram and core uncovery (8 hrs) and that the subsequent vessel failure dues not occur until 13.4 hours into the accident. The sequence timing also reflects the approach developed at ORNL to represent the events between onset of core degradation and vessel failure for BWRs (summarized in Table 3).

The blowdown from the drywell into the secondary containment after containment failure (Fig. 5) initially fails the blowout panels between the reactor building and the refueling bay and between the refueling bay and the environment which are depicted in Fig. 7. The conditions in the secondary containment (Fig. 8) are determined by the inleakage from the drywell and the fire protection system sprays which actuate automatically. The reactor building sprays are assumed to be available in Station Blackout since they have a dedicated diesel-generator.

## Fission Product Transport Analysis

Pathways for the transport of fission products correspond closely with convective flow patterns between inner reactor volumes and the outside atmosphere, as illustrated in Figure 9. In the early stages of the station blackout accident (with the reactor vessel still intact), large amounts of volatile fission products are released from over-heated or melted fuel and flow into the upper regions of the reactor vessel. Those that are not deposited flow into the wetwell with SRV actuation or are carried into the main condensers by leakage flow past the MSIVs. The latter constitutes a significant pathway for the transfer of fission products out of primary containment, although not necessarily to the atmosphere. Because small venting occurs routinely between the primary and secondary containments, small amounts of wetwell inventories may leak into the reactor building during the early stages of the accident. However, these releases are dwarfed by those from the corium-concrete reaction after drywell failure.

The release pathways after reactor vessel bottom head failure and containment failure are illustrated in the lower portion of Fig. 9. The dominant pathway transports fission products out of the drywell and into the reactor building, from which small amounts leak directly to the atmosphere. Considerably larger amounts are retained in the reactor building by dissolution in water (arising from both condensing steam and fire protection system sprays), deposition onto wetted walls, or deposition on aerosols which subsequently settle. Significant leakage from the reactor building to the refueling bay also occurs, after which the fission products either deposit in the refueling bay or are carried to the atmosphere by leakage through the blowout panels. Thus, the primary atmospheric releases are due to leakage from the reactor building and refueling bay after drywell failure.

The actual calculated fission product releases for this accident are fairly low, indicating extensive mitigation by various reactor systems and containment volumes. Considerable holdup of xenon and krypton is indicated, due primarily to the slow leakage rates from secondary containment to the atmosphere. At the end of the transient analysis (1500 min. after shutdown), only about 18% of the shutdown inventory of noble gases had reached the atmosphere, with 33% still in the secondary containment and almost 50% decayed. (In calculating shutdown inventories, only those isotopes are included which are important during the release phase of the accident, i.e., the middle and longer lived isotopes that are actually released during the sequence.)

Atmospheric releases of iodine, cesium, and tellurium constitute only 0.16%, 0.058%, and 0.40%, respectively, of the shutdown fuel inventories for these elements. It is interesting to note that these all are predicted to occur as gas phase releases, with aerosol contributions being several orders of magnitude lower. A principal repository of these elements is the pressure suppression pool, for which transient activity is shown in Figure 10. As seen in the figure, major events in the accident sequence can be noted by their effects on wetwell fission product inventories. The pressure vessel releases due to SRV actuation can be seen as stepwise increases for both iodine and cesium between 600 and 700 min. During this time, tellurium is largely retained in the fuel by reaction with zirconium, but is released in large quantities from the drywell rubble after 806 min. Subsequent drywell venting into the wetwell produces the gradual rise in the wetwell tellurium inventory between 1000 and 1100 min.

Other locations containing high levels of iodine are shown in Figure 11, where it is seen that retention by water pools and surfaces play a very important role. It should be noted in Figure 11 that 44% of the shutdown inventory of iodine is accounted for — the remainder has decayed.

## Summary

The ORNL SASA code suite is capable of comprehensive analyses of BWR severe accident sequences:

1. greatly improved in-vessel and ex-vessel thermal hydraulic modeling (BWR-LTAS and MARCON 2.1B)

2. secondary containment model (SCM) complements MARCON 2.1B by calculating the thermal-hydraulic response of secondary containment volumes and the effects of additional systems such as SGTS and fire protection sprays
3. comprehensive fission product transport code (TRENDS) with "state-of-the-art" chemistry, radioactive decay for nuclides, and all major transport and retention mechanisms for important volatile elements.

In the Station Blackout accident sequence at Browns Ferry Unit 1, there is significant time available for corrective action, for instance:

1. the core uncovers 8 hours after scram
2. fuel starts to melt 10 hours after scram
3. reactor vessel fails 13.4 hours after scram.

Even without corrective action, the fission product releases are small:

1. less than 1% of I, Cs and Te are released to the atmosphere
2. there is also significant holdup of noble gases in the primary and secondary containments.

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Table 1

MARCON 2.1B INCORPORATES MAJOR IMPROVEMENTS IN BWR UNCOVERED CORE AND PRIMARY CONTAINMENT RESPONSE CALCULATIONAL METHODOLOGY

---

- In-Vessel
  - Decay Heat by Rigorous ANS Standard
  - Canisters, Control Blades, and SRVs
  - Separation of Fuel and Cladding
  - Multi-Region Water Inventory and True Algorithm for Water Level
  - Physical Properties, Steam/Gas Equation of State
  - Accurate Pressure Calculation, Core Quench Models, Boiling and Flashing Algorithm
  - Heat Transfer Correlations for Uncovered Core
  - $B_4C$/Steam Reaction Models
  - Limited Melt Relocation Models
  - Bases for Vessel Failure
- Ex-Vessel
  - Reactor Vessel Heat Source To Drywell
  - Drywell Sump Models
  - Continuity of Mass and Volume at Vessel Failure
  - CORCON MOD 2
  - Temperature Dependent Specific Heat
  - Pressure Dependent Correlation for Superheat Temperatures
  - Degassing of Concrete in Drywell

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Table 2

MAJOR ACCIDENT EVENTS AFTER CORE UNCOVERY

| Event | Time after scram (mins) |
|---|---|
| Core uncovery | 479 |
| Structural relocation starts | 572 |
| Fuel melting starts | 604 |
| Core plate dryout | 630 |
| Core plate failure | 682 |
| Core collapse | 695 |
| Bottom head dryout | 709 |
| Penetration failure | 734 |
| Vessel pressure equalizes with containment | 743 |
| Corium leaves vessel | 797 |
| End Hotdrop/start Corcon | 800 |
| Containment failure | 805 |

Table 3

ORNL METHODOLOGY EMPLOYED TO REPRESENT THE EVENTS BETWEEN ONSET OF CORE DEGRADATION AND VESSEL FAILURE FOR BWRS

- Molten canisters and control blades relocate onto core plate which causes
  - dryout of core above core plate, and
  - steaming increased before dryout, stopped until core plate failure
- Core plate fails at 1275°F (964 K) subsequently
  - Debris falls into bottom head
- Remaining intact core collapses when molten fraction exceeds specified amount (currently 50%)
- Bottom head dryout
- Penetration failure at debris temperature of 2800°F (1811 K), thus
  - vessel depressurizes, until
  - vessel pressure equilizes with containment
- corium liquidus [after heatup to ~3500°F (2200 K)] leaves vessel

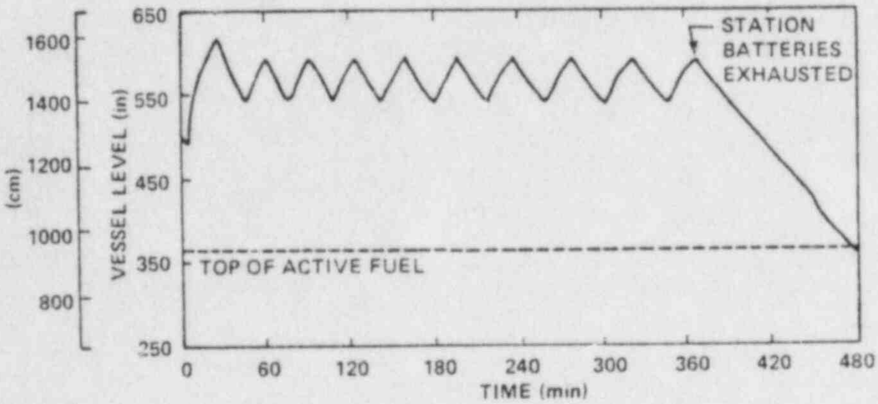


Fig. 1. Reactor vessel water level in the initial phase of the accident (prior to core uncovery).

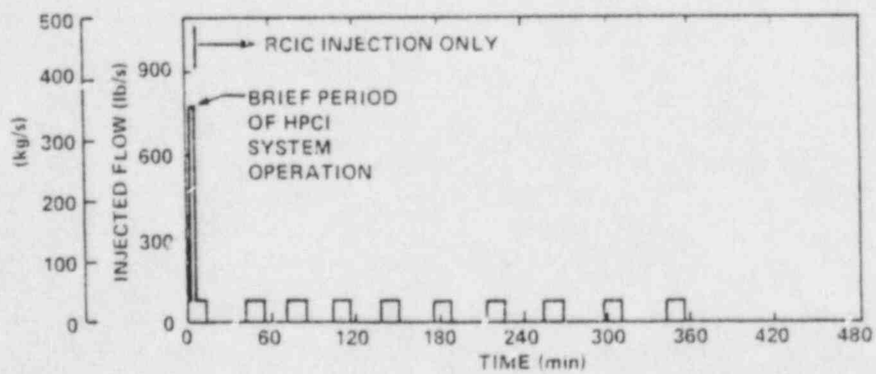


Fig. 2. Reactor vessel injection flow in the initial phase of the accident (prior to core uncovery).

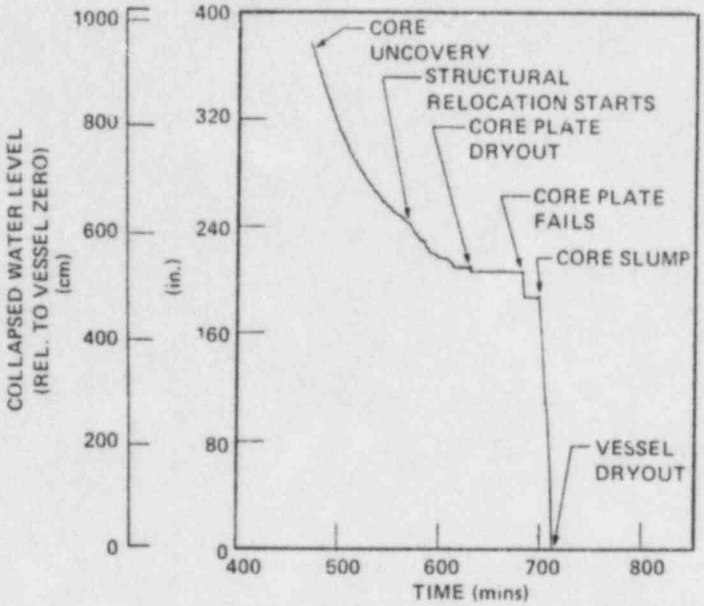


Fig. 3. Reactor vessel water level after core uncovery.

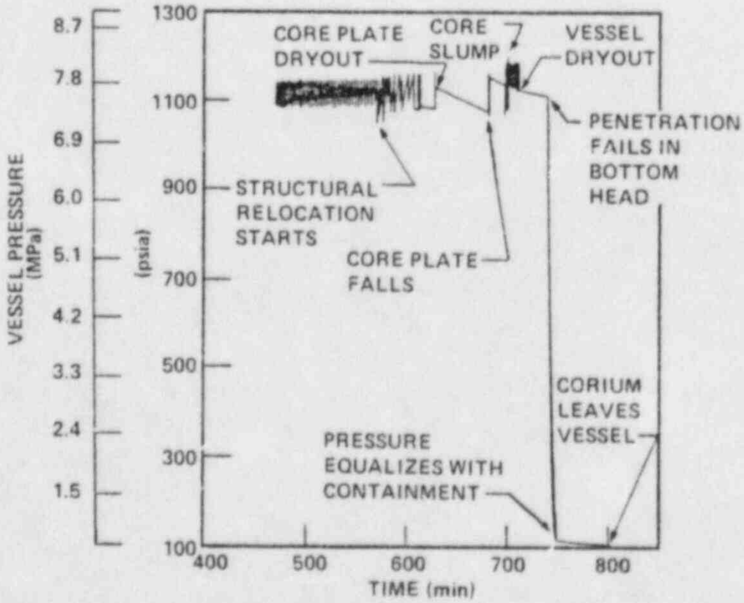


Fig. 4. Reactor vessel pressure after core uncovery.

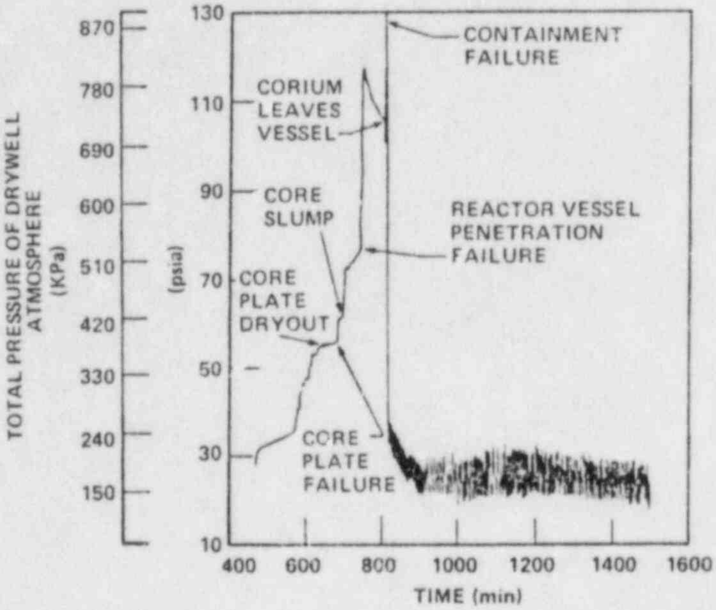


Fig. 5. Drywell pressure after battery exhaustion.

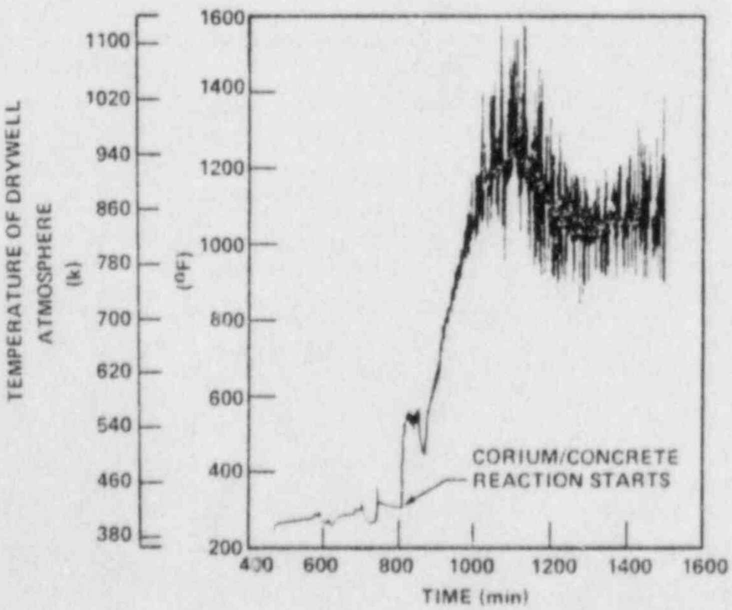


Fig. 6. Drywell atmospheric temperature after battery exhaustion.

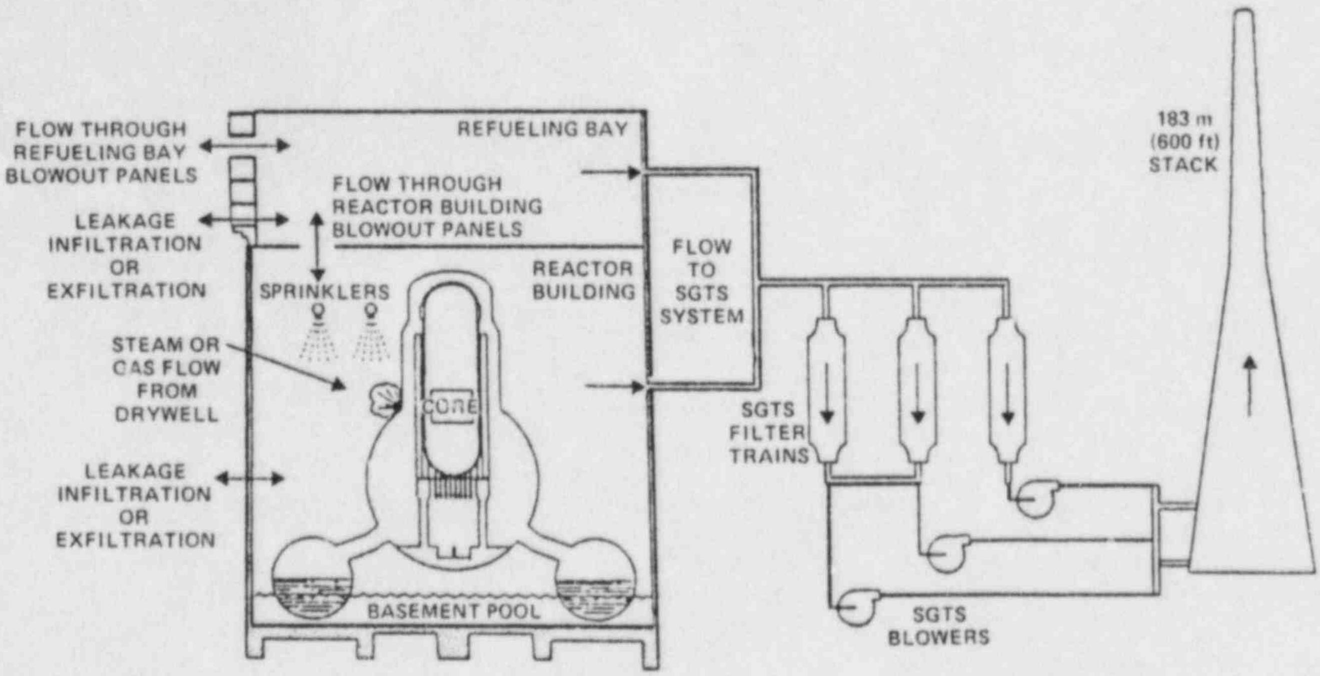


Fig. 7. The Mark I secondary containment design with indicated response after drywell failure (the SGTS does not operate in the Station Blackout Sequence).

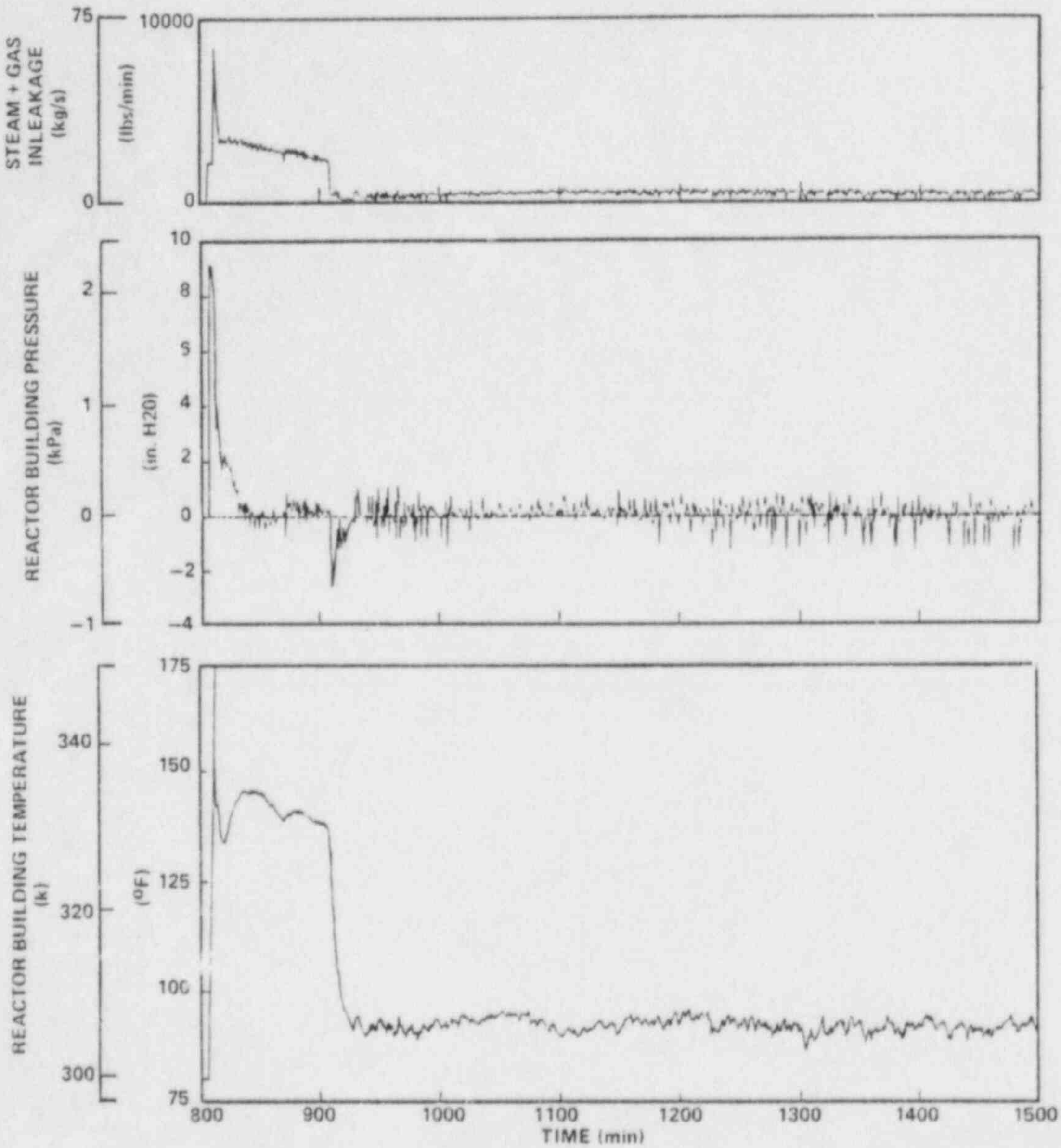


Fig. 8 Reactor building pressure and temperature response caused by steam and gas inleakage from drywell.

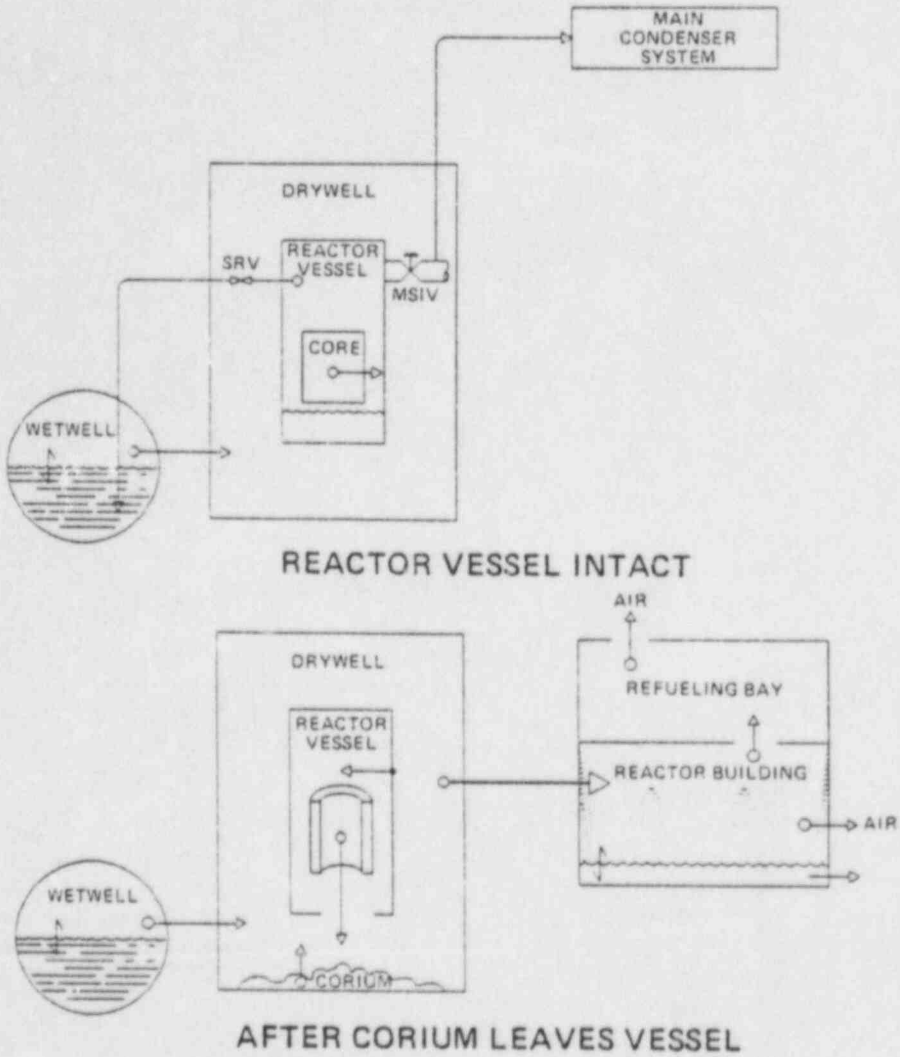


Fig. 9. Fission product pathways for Station Blackout.

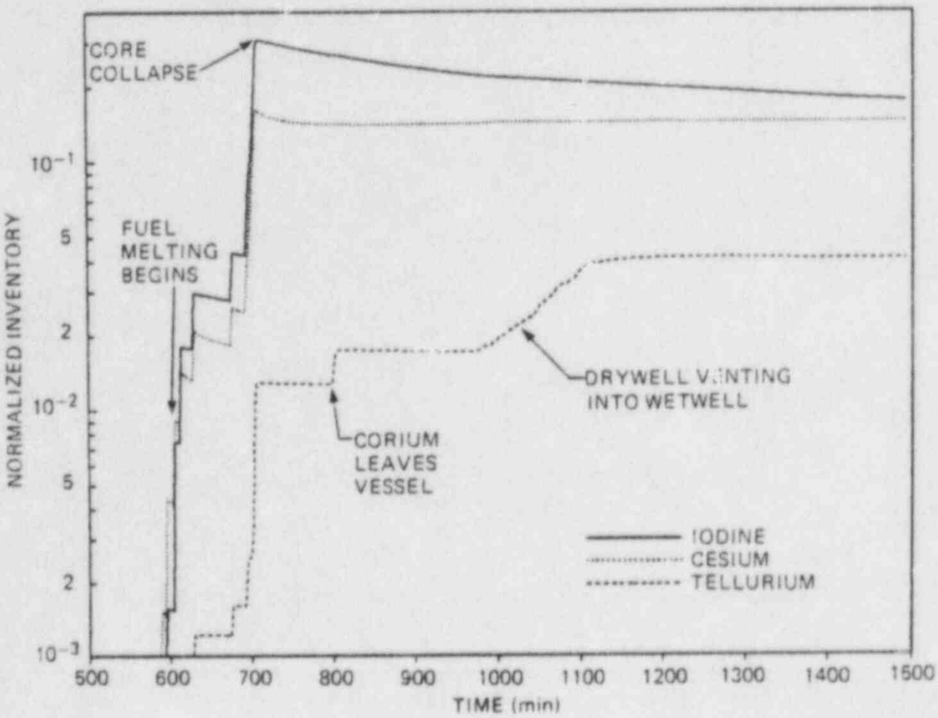


Fig. 10. Transient inventory of I, Cs, and Te in suppression pool.

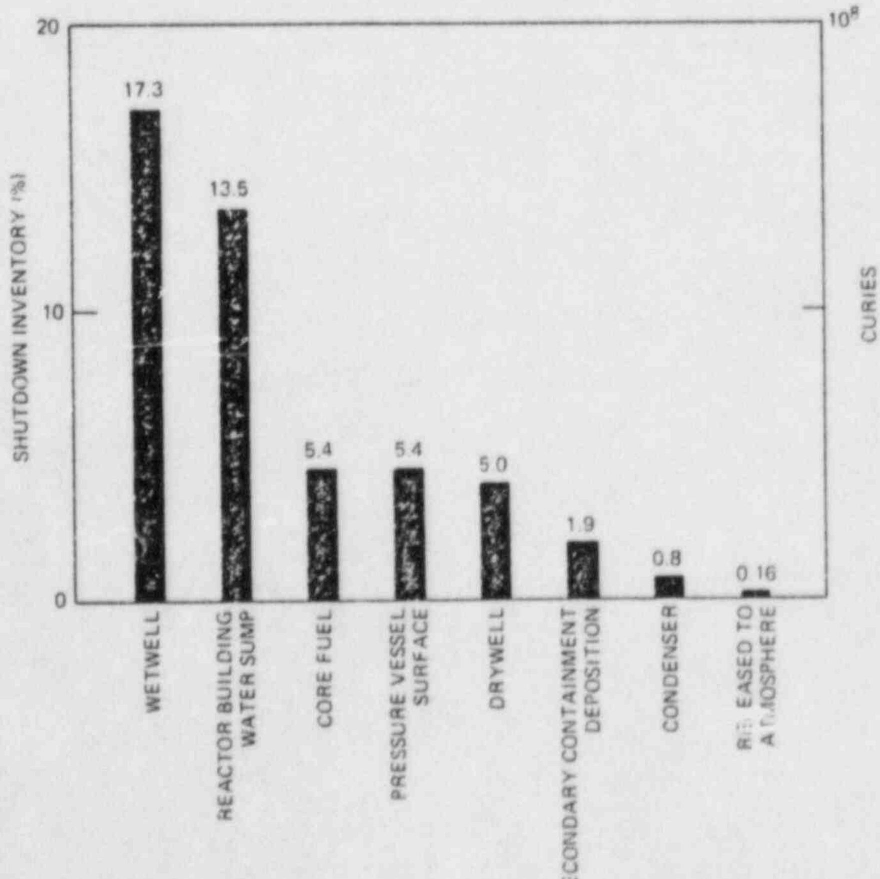


Fig. 11. Principal iodine repositories at end of transient (note that of the shutdown inventory has decayed).

# STATION BLACKOUT CALCULATIONS FOR PEACH BOTTOM

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## ABSTRACT

A calculational procedure for the Station Blackout Severe Accident Sequence at Browns Ferry Unit One has been repeated with plant-specific application to one of the Peach Bottom Units. The only changes required in code input are with regard to the primary containment concrete, the existence of sprays in the secondary containment, and the size of the refueling bay. Combustible gas mole fractions in the secondary containment of each plant during the accident sequence are determined. It is demonstrated why the current state-of-the-art corium/concrete interaction code is inadequate for application to the study of Severe Accident sequences in plants with the BWR MK I or MK II containment design.

## INTRODUCTION

Best estimate studies for BWR Severe Accident sequences based upon Browns Ferry Unit One have been conducted under the auspices of the Severe Accident Sequence Analysis (SASA) Program at Oak Ridge National Laboratory since October 1980.[1-8] The program has enjoyed the full cooperation and assistance of the Tennessee Valley Authority, who have provided the necessary basic information concerning the design and operation of Browns Ferry as well as invaluable informed peer review of the SASA program reports.

Code development has never been a primary goal of the SASA effort, but it became evident at the inception of the program that improvement of the existing codes, all structured for PWR accident analyses, was necessary if the BWR studies done at ORNL were to be meaningful. The initial ORNL study was Station Blackout at Browns Ferry,[1,2] most of which was performed with the existing PWR codes. Now, after four years of gradual implementation and testing of BWR models during periods

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between the performance of other BWR accident sequence analyses, we believe that we finally have codes adequate for BWR accident studies, at least to the point of reactor vessel bottom head failure.[9,10]

Accordingly, calculations intended to replace those of the original Browns Ferry Station Blackout Accident Sequence study of 1981 have been performed.[10] In addition, because the Peach Bottom nuclear units are very similar to those at Browns Ferry, and because there is currently a great deal of interest in Peach Bottom source term calculations, the NRC SASA program technical monitor requested that the codes employed for Browns Ferry Station Blackout be immediately exercised again, with the necessary changes to input, for Peach Bottom Station Blackout.

## Code Input Changes for Peach Bottom

The Peach Bottom units and the Browns Ferry units are of the General Electric Company BWR 4, MK I containment design and, for the purposes of Severe Accident calculations, are practically identical. Nevertheless, there are three differences important to the potential for fission product transport in the unlikely event that a severe accident should occur:

1. The primary containment concrete at Peach Bottom has a high limestone content and a much higher potential for gassing when heated by corium than does the concrete structure of the Browns Ferry units (see Table 1).

2. The secondary containments of the Peach Bottom units do not employ general area fire protection system sprays. The Browns Ferry units have a highly reliable system that would, even without operator action, and even without electrical power, provide overhead sprays into the reactor building of any unit in which the primary containment had failed.

3. Each unit at Peach Bottom stands alone whereas the units at Browns Ferry abut and share a common refueling bay. Therefore, assuming a severe accident at just one unit in both cases, the refueling bay volume interposed between the reactor building and the atmosphere at Peach Bottom would be one-third of that at Browns Ferry (see Fig. 1).

## Fission Product Transport Results

The major fission product transport pathways for the Peach Bottom Station Blackout accident sequence are shown in Fig. 2. During the first phase of the accident sequence when the reactor vessel is intact and the primary containment has not failed, the only significant fission product escape is via main steam isolation valve (MSIV) leakage to the main condensers in the turbine building. Within the primary containment, fission products move from the reactor vessel to the pressure suppression pool via the safety/relief valves (SRVs). The noble gases and the very small fraction of the other fission products that escape the pool can pass back from the wetwell atmosphere into the drywell via the vacuum breakers. The largest fission product inventory outside of the reactor vessel would be in the pressure suppression pool. The pool is heated as the steam relieved from the reactor vessel is condensed; evaporation from the pool surface causes the pressure in the primary containment to increase. However, most of the primary containment pressure increase is due to the presence of non-condensible gases generated by metal-water reactions within the reactor vessel.

If the severe accident progresses to the point where the reactor vessel bottom head fails and molten corium pours onto the drywell floor, then the water in the drywell sumps is quickly evaporated and the containment approaches its failure pressure. Shortly after the corium-concrete interaction begins, the additional releases of non-condensible gases causes the drywell liner to fail and the containment depressurizes into the reactor building. Blowout panels open between the reactor building and the refueling bay, and between the refueling bay and the atmosphere.

As shown on Fig. 2, the major fission product escape pathway after the reactor vessel bottom head and the primary containment have failed is from the drywell via the reactor building and the refueling bay to the atmosphere. The principal fission product removal mechanism along this pathway would be settling and deposition, but it should be noted that the interior walls and other surfaces of the reactor building and, to a lesser extent, the refueling bay would be wetted by the condensation of steam.

Fission products would enter the drywell from three sources. First, depressurization of the containment would cause boiling and flashing of the pressure suppression pool, and fission products in the wetwell atmosphere would be carried into the drywell via the vacuum breakers along with the steam. Second, fission products would be released from the interior of the failed reactor vessel into the drywell. Third, fission products sparged from the corium mass on the drywell floor by the gases produced by the corium-concrete interaction would directly enter the drywell atmosphere.

It is important to recognize that two of the sources of fission products to the drywell are continuing generators. It is fairly obvious that the release from corium would be continuous, but it should also be noted that a significant amount of fuel would remain in the reactor vessel after bottom head failure and would not release fission products until long thereafter. This late-failing fuel would come from the outer radial zone of fuel, which has a very low power factor, and is represented schematically in the lower portion of Fig. 2.

The codes used in support of the Peach Bottom fission product transport calculations are the same codes that were used for the Browns Ferry Analysis,[10] with changes in code input as necessary to reflect the plant specific differences discussed in the preceding section. Cliff Hyman provided the input changes regarding concrete composition for MARCON 2.1B. The Secondary Containment Model (SCM) input required changes to reflect the absence of sprays and the smaller volume of the refueling bay. Chuck Weber utilized the results of the MARCON 2.1B and SCM calculations for Peach Bottom to perform QUICK calculations for the behavior of aerosols in the secondary containment and combined the results of all of these calculations as necessary to drive the TRENDS code for the Peach Bottom Station Blackout accident sequence.

The fission product transport portion of the overall ORNL SASA program effort is of limited scope. At the current stage of development, only the noble gases and isotopes of iodine, tellurium, and cesium are included in the transport calculations. Most of the overall SASA program effort at ORNL is dedicated to providing and improving methods for dealing with the thermal-hydraulic aspects of BWR Severe Accident sequences. It is the purpose of the relatively small fission product transport effort to produce first-approximation calculations that are used to indicate the sensitivities of the analyses and to determine which thermal-hydraulic model improvements are most needed.

Comparison of the TRENDS code results for the Peach Bottom and Browns Ferry Station Blackout accident sequences indicates that the fission product releases at the secondary containment boundary would be significantly higher at Peach Bottom. Specifically, at the end of the calculations, the ratio of Peach Bottom release to Browns Ferry release is 9.25 for the iodine, 7.83 for the tellurium, and 2.33 for the cesium isotopes. Both calculations were carried out as necessary to represent a period of 25 hours following the loss of all ac power and reactor scram. The major contributing factors to the higher releases for Peach Bottom are the higher gas blowing rates from the Peach Bottom concrete and the absence of reactor building atmospheric sprays, although the much higher steam condensation rate at Peach Bottom partially compensates for the lack of atmosphere sprays. (The iodine deposition on the reactor building walls at Peach Bottom is predicted to be 4.13 times that for Browns Ferry.)

The distribution of noble gases at the end of the calculations also shows a greater holdup factor for the Browns Ferry containment systems than for Peach Bottom. The ratio of calculated Peach Bottom release to Browns Ferry release is 2.44 for the noble gases, and this difference is believed to be entirely due to the higher gas blowing rates from the Peach Bottom corium/concrete interaction, which flush the noble gases from the secondary containment.

Thus the TRENDS code calculations predict that the plant-specific differences between Browns Ferry and Peach Bottom would lead to higher releases at Peach Bottom for the same accident sequence; review of the differences and the calculated results indicates that this conclusion is reasonable given that the calculational methodology is correct. Nevertheless, the calculational methodology must be acknowledged to be incorrect in two important areas. The potential for combustible gas burns in the secondary containment has been neglected and the basic model for the geometry of the corium/concrete interaction is wrong.

## Combustible Gases in the Secondary Containment

The hydrogen produced by metal-water reactions both within the reactor vessel and on the drywell floor and the carbon monoxide produced as a byproduct of the corium/concrete interaction are of sufficient quantities (see Table 2) that it is necessary to consider the possibility of combustible gas burning in the secondary containment after primary containment failure. (The primary containment is inerted.) Combustible gas burning is considered possible if the combined mole fraction of hydrogen plus 60% of the carbon monoxide in the atmosphere is greater than 0.09, the mole fraction of oxygen is greater than 0.05, and the mole fraction of inerting fluids (steam plus carbon dioxide) is less than 0.55.[14]

As shown on Fig. 3, the mole fraction of combustible gases in the reactor building for Browns Ferry Station Blackout would exceed 0.09 shortly after drywell failure, which occurs 805 minutes after reactor scram. Also, the oxygen mole fraction remains above the lower limit for combustion and the mole fraction of inerting gases does not reach the inerting limit. If an ignition source were present, burning would be expected. Of course, there would be no electrical power and all surfaces would be wetted because of the sprays. Although not shown, the conditions in the Browns Ferry refueling bay would also permit burning after time 1196 minutes, when the combustible gas mole fraction exceeds 0.05 there.

The relatively high oxygen mole fraction and the relatively low mole fraction of inerting gases in the Browns Ferry reactor building are caused by the steam-condensing action of the fire protection system sprays, which also have the highly beneficial effect of mitigation of fission product transport. The calculated mole fractions for the Peach Bottom reactor building, which has no atmosphere sprays, are shown in Fig. 4. Except for a period of about 35 minutes shortly after drywell failure, there is insufficient oxygen to support combustion. Also, the reactor building atmosphere is inerted by steam and carbon dioxide during the period 850 to 1083 minutes after scram. However, parallel calculations for the Peach Bottom refueling bay (results not shown) indicate that conditions there would support combustion from shortly after drywell failure throughout the end of the calculated sequence.

It is beyond the scope of this paper to discuss the effect of combustible gas burns in the reactor building or refueling bay should they occur. The CONTAIN code has been made operational on the ORNL CRAY computer system and an input deck for the Browns Ferry secondary containment has been constructed. It is intended that the effect of combustible gas burning will be included in future calculations.

## Uncertainty in Corium/Concrete Interaction Calculations

The most important remaining area of uncertainty in Severe Accident calculations for BWR MK I and MK II containments is the effect of the corium/concrete interactions. The state-of-the-art code is CORCON MOD 2, developed at Sandia National Laboratory to replicate observed experimental results for molten corium poured into concrete crucibles.[11] In general, the code maintains the corium in a roughly cylindrical geometry, making allowances for the crucible cavity to be eroded by both radial and downward attack. This approach is adequate for PWR calculations and for BWR MK III calculations in which a cylindrical cavity underneath the reactor vessel in actuality would catch the emergent corium in the event of a Severe Accident. It is not, however, adequate for BWR MK I and MK II containment studies.

The model geometry employed in CORCON does not represent the relatively flat BWR MK I or MK II drywell floor, nor does it permit a realistic assessment of the effect of the corium, spread over the drywell floor rather than collected in a cylindrical cavity. As shown on Fig. 5, the volume within the reactor pedestal underneath the reactor vessel in the MK I containment does appear to a cylindrical cavity when viewed from the side. Nevertheless, as shown in the unwrapped view of the pedestal, there is one open doorway through the pedestal wall which has a lower edge flush with the drywell floor. Therefore, while frozen corium should be retained within the pedestal region, molten corium would be expected to run out the doorway and spread over the annular drywell floor outside of the pedestal wall.

To understand the disposition of corium on the drywell floor, it is first necessary to consider the mode of ejection from the BWR reactor vessel. More work needs to be done in this area, but current models employed at Oak Ridge in the MARCON 2.1B code represent the following sequence of events after the onset of severe core degradation.

Molten canister and control blade materials relocate onto the core plate, causing increased steaming before core plate dryout. Steaming then stops until core plate failure, which is assumed to occur when the average temperature of the core plate and accumulated debris reaches 1275 F (964K). The debris then falls into the reactor vessel bottom head. The remaining intact portions of the core are assumed to collapse when the molten fraction of fuel reaches 50%. The remaining water in the lower head is boiled away, resulting in bottom head dryout with an accumulation of frozen debris at about 2475 F (1630K). The debris reheats, and penetration failure is assumed to occur at a debris temperature of 2800 F (1811K). The vessel depressurizes, and the vessel pressure equalizes with the containment pressure. The corium becomes liquidus after reheat to 3500 F (2200K) and the corium runs out of the vessel.

The corium would first fall onto the drywell floor within the confines of the reactor pedestal. About 13% of the volume of the total core would be retained in the two drywell sumps that are located immediately beneath the bottom of the reactor vessel, and there would be an additional cooling effect due to the interaction of the corium with the sump water. Thus the CORCON calculation begins with the corium at a relatively low temperature. The metallic layer is molten but the oxide layer is frozen. The frozen oxide would be expected to remain within the pedestal area but the molten metallic portion should run out the pedestal doorway onto the outer drywell floor. This lateral separation of the metallic and the oxidic layers cannot now be represented with CORCON.

There are additional chemical reactions that should be modeled in CORCON for calculations in which the code is to be applied to BWR severe accident analyses. The BWR corium would include a large amount of unreacted zirconium. The zirconium metal can reduce uranium dioxide to uranium metal, thereby increasing the density of the metallic layer.[12] This effect is not included in CORCON. The BWR corium would also contain boron carbide from the control rod power. The chemical impact of this has the potential to be significant, but is not modeled in CORCON.

Although the MARCON 2.1B models currently include representation of a general core slump when a user-input fraction of the fuel has become molten, other ORNL SASA program calculations using MELRPI indicate that BWR corium would not leave the reactor vessel all at once. The first release would comprise about half of the core, followed by a continuous but relatively slow pouring of the remainder of the core over a period of hours. CORCON does not permit a time-dependent release.

In the Station Blackout accident sequence, the reactor vessel would have failed and the corium would be on the drywell floor at the time of primary containment failure. The primary containment is expected to fail by overpressure[10] with the failure location in the drywell liner, at the juncture of the cylindrical and spherical sections.[13] Steam and gases escaping from the liner would enter the approximately 2-in. (5.08 cm) gap between the liner and the concrete shield and then pass through the many holes in the concrete shield into the reactor building. Thus, after drywell failure, fission products present in the drywell atmosphere can escape directly into the reactor building. As shown schematically in Fig. 6, fission products in the reactor building can, to a small extent, leak to the surrounding atmosphere, but most of the flow would be via blowout panels to the refueling bay and from the refueling bay through blowout panels to the atmosphere.

Since the fission product release associated with the corium-concrete interaction has the potential to bypass the pressure suppression pool, its relative importance on a per-unit weight basis is at least two orders of magnitude greater than that of the fission product releases during the period when the reactor vessel is intact. Accordingly, provision should be made to upgrade the CORCON code for BWR applications. This is believed to be by far the single most important remaining area of uncertainty in BWR Severe Accident Calculational Methodology.

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Table 1. Composition of Peach Bottom and Browns Ferry concretes.

| | PEACH BOTTOM | BROWNS FERRY |
|---|---|---|
| $Al_2O_3$ | 0.016 | 0.018 |
| CaO | 0.454 | 0.310 |
| $CO_2$ | 0.357 | 0.200 |
| $SiO_2$ | 0.036 | 0.388 |
| $H_2O$ EVAP | 0.039 | 0.048 |
| $H_2O$ CHEM | 0.020 | 0.017 |
| OTHER | 0.078 | 0.019 |

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Table 2. Total combustible gas production to 25 hours after reactor scram for Peach Bottom Station Blackout

Hydrogen

| | | |
|---|---|---|
| In-vessel | 1,576 lbs | (714 kg) |
| Drywell floor | 1,494 lbs | (678 kg) |

Carbon Monoxide

| | | |
|---|---|---|
| In-vessel | 2 lbs | (1 kg) |
| Drywell floor | 54,078 lbs | (24,514 kg) |

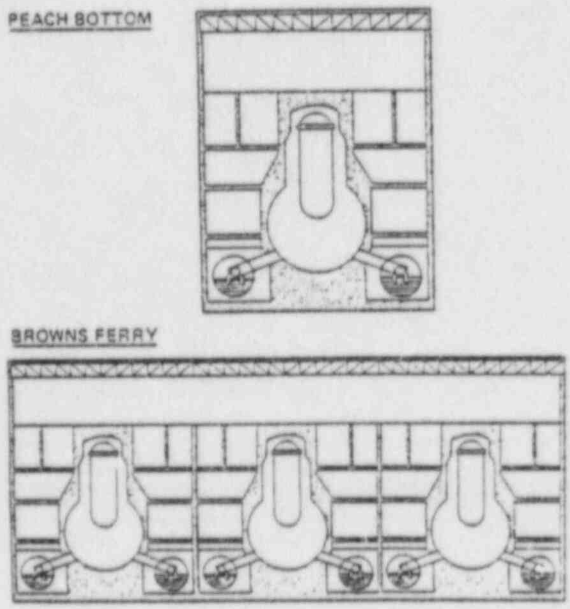


Fig. 1. The refueling bay volume per unit reactor building volume is three times greater at Browns Ferry than at Peach Bottom.

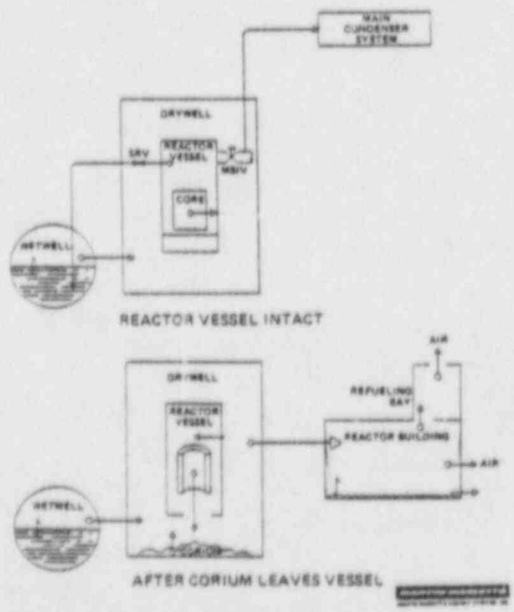


Fig. 2 Fission product transport pathways for Peach Bottom Station Blackout. The drywell liner is predicted to fail shortly after failure of the reactor vessel bottom head.

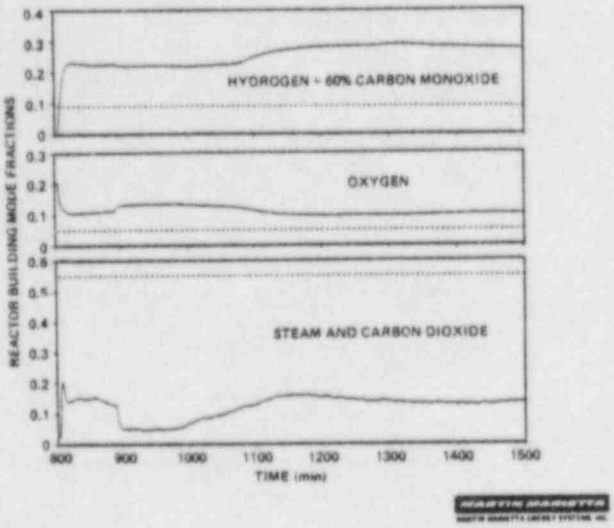


Fig. 3. Calculations indicate that for Station Blackout at Browns Ferry, combustible gas burning could occur in the secondary containment after primary containment failure if an ignition source is available.

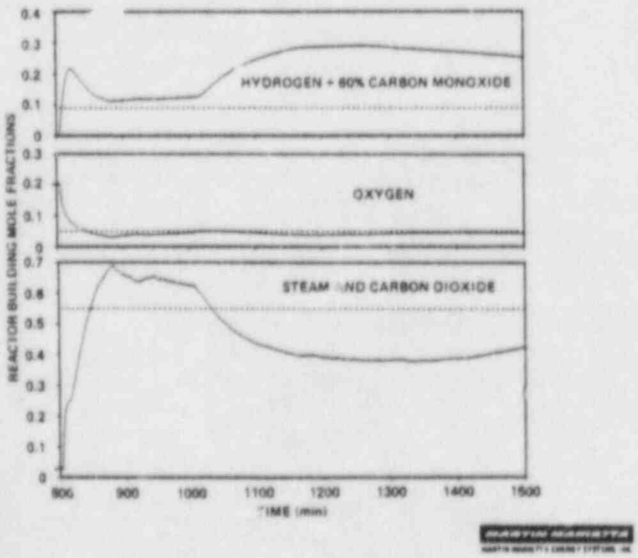


Fig. 4. After primary containment failure, there is insufficient oxygen in the Peach Bottom Reactor Building to permit combustible gas burning during most of the remainder of the Station Blackout sequence.

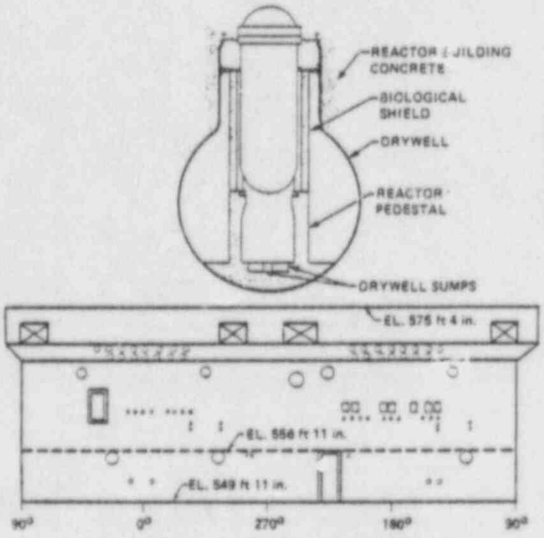


Fig. 5. Structure of the BWR MK I containment and an unwrapped view of the reactor pedestal, showing the doorway that, in the event of a Severe Accident, would permit exodus of the molten portions of the corium onto the general drywell floor.

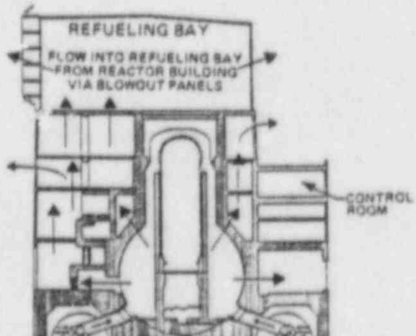


Fig. 6. After drywell failure, steam and gases in the drywell atmosphere can enter the secondary containment directly, without passing through the pressure suppression pool.

MELRPI — DEVELOPMENT AND USE

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## ABSTRACT

The MELRPI computer code has been developed by Rensselaer Polytechnic Institute under the sponsorship of Oak Ridge National Laboratory (ORNL) and, more recently, the Empire State Electrical Energy Research Corporation (ESEERCO). The code was developed especially for severe accident analyses concerning BWRs and is not applicable to PWRs. MELRPI.MOD2, one of the ORNL-severe accident analysis codes, has been applied for the first time to station blackout transient analysis for the Browns Ferry nuclear power plant in order to estimate the progression of core degradation.

## 1. DESCRIPTION OF MELRPI.MOD2 MODELS

The MELRPI.MOD2 computer code includes core degradation and emergency core cooling system (ECCS) models. Simplified mechanistic approaches have been used in developing the code so that inexpensive calculations can be performed.

The 2-D reactor core geometry presented in Fig. 1 is composed of fuel rods, cruciform control blades, channel boxes, and main and bypass channels. Mechanistic core degradation models simulate heatup, oxidation, melting, relocation, and freezing of molten materials, clad failure, and rubble bed formation. Rubble bed formation can progress in both the axial and radial directions resulting in the reduction of core height in individual radial zones.[1]

MELRPI-ECCS models provide a mechanistic approach for the thermal hydraulic analysis of a boiling water reactor core with intact and debris bed nodes.[2] A schematic of the reactor core model is presented in Fig. 2 with arrows in the figure indicating flow paths. Bottom flooding, interstitial injection, and core spray ECCS models are

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available. A single uniform water level is employed in the interstitial region, but the level can vary radially in the main channels, where most of the decay heat is transferred to the coolant.

Current model development activities concentrate on lower core plate failure, response of the structures in the lower plenum, lower head failure, in-vessel thermal hydraulics, and improved debris bed thermal hydraulics. The lower plenum and lower head failure models will be used in analyzing corium interaction with control rod guide tubes, water, and the reactor vessel bottom head.

Future improvements include eutectic mixture formation, flow channel blockage due to relocated and frozen materials in cold portions of the core, and improvements to the existing radiation heat transfer models.

## 2. APPLICATION TO STATION BLACKOUT

The calculations described in this study pertain to station blackout at Browns Ferry unit 1 whose core geometry and characteristics are described in detail in Refs. 3 and 4. For the calculational model, the core is divided into 5 equal-length axial nodes and 5 radial zones (Fig. 5). The radial and axial power profiles used in the calculation reflect the Browns Ferry equilibrium core. The volumetric fraction assigned to each radial zone was selected so as to best represent the radial power profile.

The station blackout transient has been analyzed by utilizing several computer codes at ORNL. The period prior to the onset of core uncovery has been analyzed by the BWR-LTAS code. MARCON2.1B is used for the remainder of the transient along with the SCM and TRENDS codes; however, MARCON does not have mechanistic core deformation and relocation models. MELRPI attempts to provide more insight into this portion of the transient by deforming the core, relocating and removing molten materials gradually. MELRPI.MOD2, which uses the rigorous ANS-standard decay heat curve,[5] has been coupled with MARCON2.1B from the onset of core uncovery until core plate failure occurs. (The core plate failure is predicted by MARCON2.1B.) MARCON2.1B results are used to drive MELRPI, supplying the two-phase level, reactor vessel pressure and core steam flow history presented in Figs. 3 and 4. In turn, MELRPI results are used to adjust MARCON2.1B input for better representation of core degradation phenomena.

## 3. RESULTS

A description of the station blackout severe accident sequence for Browns Ferry together with the calculations of the MARCON2.1B, SCM and TRENDS codes is provided in an accompanying paper in these proceedings entitled "Station Blackout Calculations for Browns Ferry." The following results pertain to the MELRPI calculation for station blackout.

Core degradation results are presented in Figs. 5–9. The rate of fuel temperature increase in the central radial zone is reduced after Zircaloy melting begins, mainly due to inhibition of the exothermic oxidation reaction between Zircaloy and steam (Fig. 6). Temperatures remain relatively low until nodes become uncovered completely. Figs. 5, 7 and 8 demonstrate the level of core degradation at 519, 544, and 654 minutes after scram. Temperature contours indicate the thermal state of the core structure. Locations where debris beds are formed and fuel melting occurs are also indicated. The reduced height of the individual radial zones of the core reflects the effects of debris porosity beds and the amount of molten masses removed from these zones. Currently, molten masses are removed instantaneously upon debris bed formation and are not included in the available total mass for calculating radial zone heights. Future plans include modeling of flow blockage and incorporation of frozen materials in the calculation of radial zone heights.

Large amounts of debris and molten material are formed before the lower core plate failure as indicated in Fig. 9. In radial zones 1 and 4, 40% to 57% of the fuel becomes molten although no fuel melting occurs in the peripheral zone. The molten fractions for zone 2 and 3 remained close to the value calculated for the central radial zone. About 63% of the Zircaloy structure of the channel walls and cladding becomes molten. The fraction of core rubblized and the fraction of core height remaining covered with water are also indicated in Fig. 9.

## 4. SUMMARY

MELRPI.MOD2 has been applied to the station blackout transient in a coupled mode with MARCON2.1B for the first time. The results are used to adjust input parameters of MARCON2.1B for better simulation of core degradation phenomena and to provide additional insight into the progression of core damage before core plate failure occurs. With the inclusion of additional models, predictive capabilities of MELRPI will be enhanced in the future.

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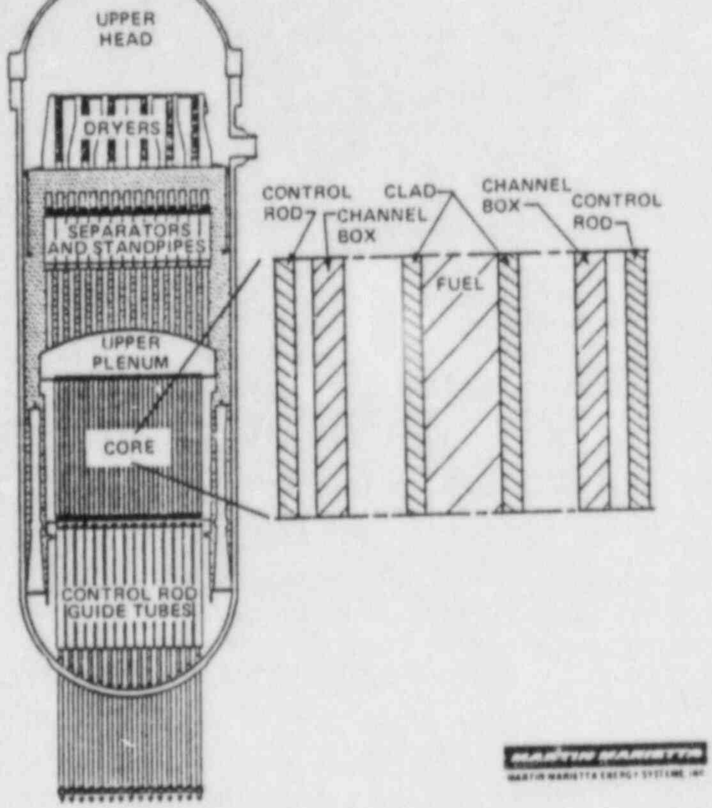


Fig. 1. MELRPI uses a two-dimensional BWR core nodalization scheme.

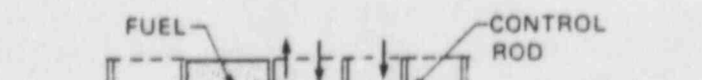


Fig. 2. A schematic of the ECCS model geometry.

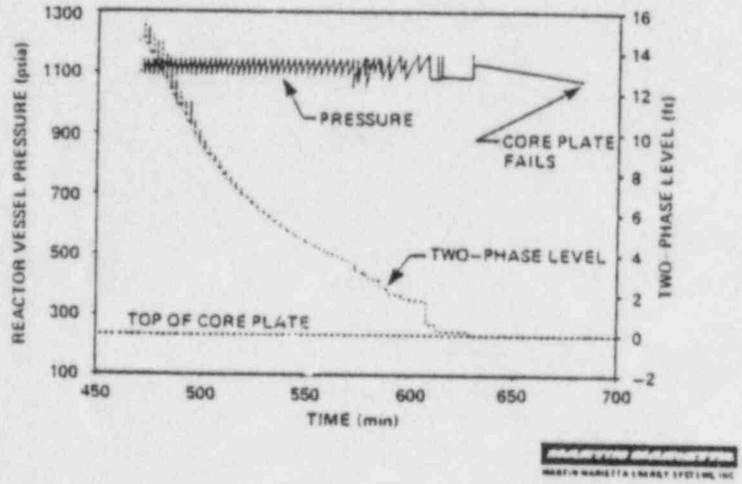


Fig. 3. Pressure and two-phase level histories as predicted by MARCON 2.1B.

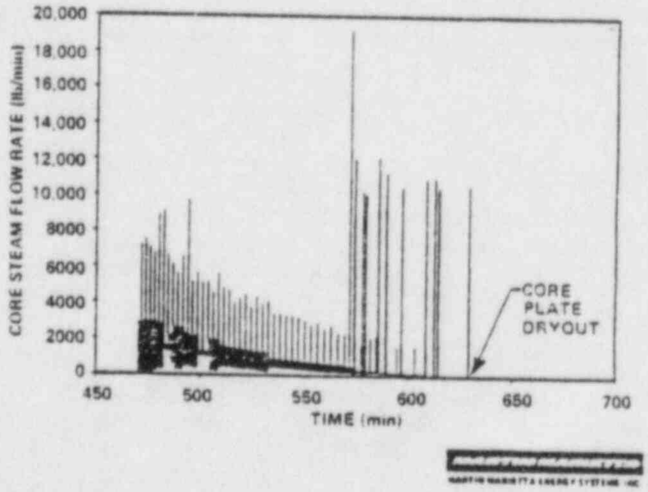


Fig. 4. Core steam flow rate as predicted by MARCON 2.1B.

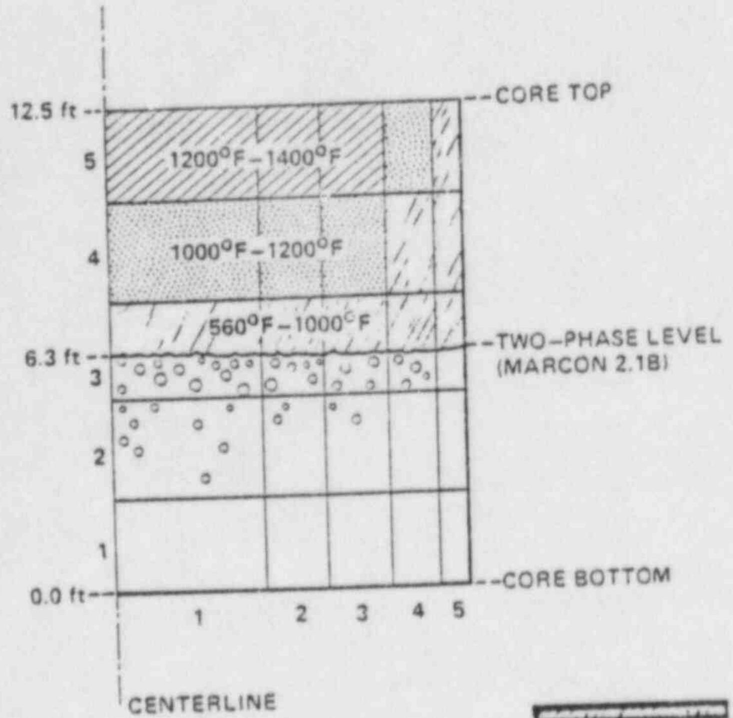


Fig. 5. The predicted state of the degraded core at time 519 minutes after scram.

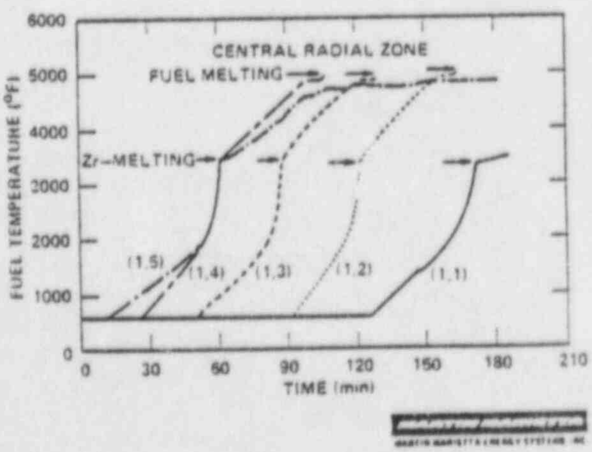


Fig. 6. Fuel temperatures of five axial nodes in the central radial zone.

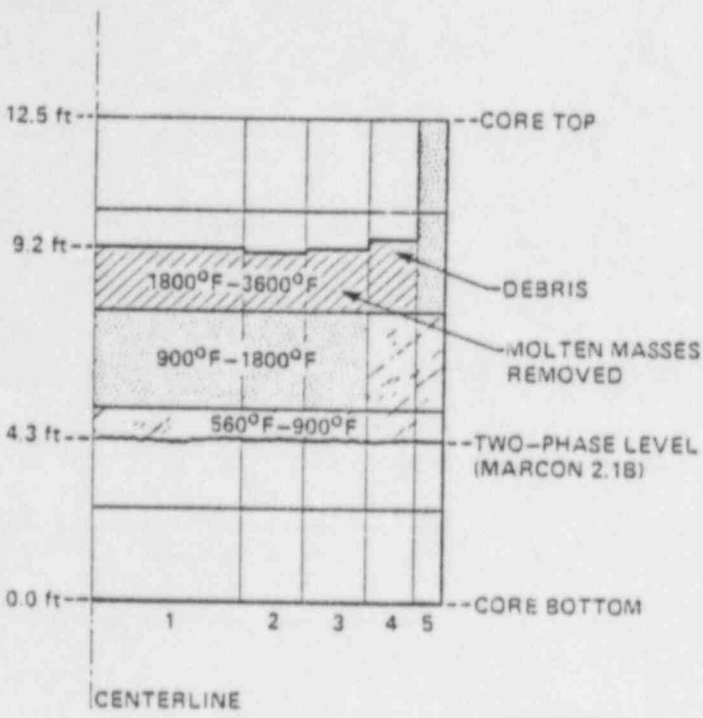


Fig. 7. The predicted state of the degraded core at time 544 minutes after scram.

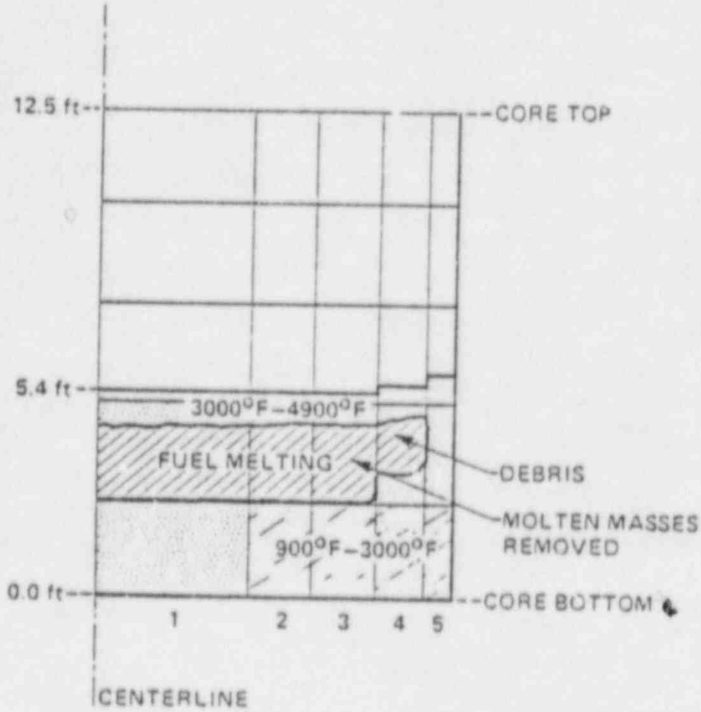


Fig. 8. The predicted state of the degraded core at time 654 minutes after scram.

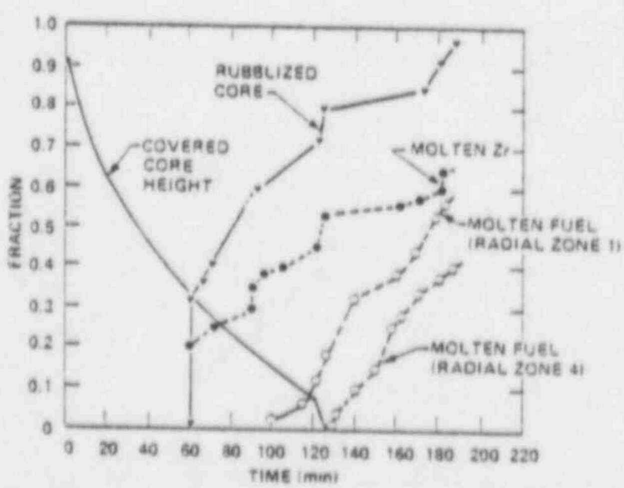


Fig. 9. Covered core height, rubblized core, molten Zr, and molten fuel fractions vs. time for Browns Ferry Station Blackout.

SAND85-1787C

# Hydrogen Transport in a Large, Dry Containment for Selected Arrested Sequences*

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ABSTRACT

The hydrogen burn in the containment during the accident at Three Mile Island instigated renewed attention to the possible consequences of hydrogen generation during a nuclear core uncovery accident. To assist in providing a basis for a recommendation on rulemaking for pressurized water reactors (PWRs) with large, dry containments, the containment environments created during arrested sequences having a 75% metal-water reaction were calculated. A small break loss-of-coolant accident and a loss of offsite power were the initiating events for the sequences analyzed. The HECTR computer code was used to calculate the containment response. Since the primary focus of this analysis was the investigation of the potential for the formation of local detonable mixtures of hydrogen, steam, and oxygen, a large number of compartments (up to 42) were used in the HECTR analysis to represent the containment geometry. The highest hydrogen concentration was always calculated in the compartment into which hydrogen and steam were released. A detonable mixture was calculated in one case where hydrogen and steam were released into a steam generator tunnel compartment. This case simulated a break in the coolant line from the core flood tank to the reactor vessel. Further analysis is required to determine the likelihood of this event, the potential for detonation in this small tunnel volume, and its effects on containment or equipment.

## Introduction

The Three Mile Island accident contributed to hydrogen control rules for pressurized water reactors (PWRs) with ice condenser containments and boiling water reactors with Mark III containments. A hydrogen control rule for PWRs with large, dry containments is presently deferred.

At the request of the Nuclear Regulatory Commission, analyses were performed to support a regulatory position on hydrogen control for large, dry containments. The specific analyses requested were to provide sufficient multi-compartment analyses to determine whether local detonable mixtures can be formed in large, dry light water reactor containments, which, given an ignition source, can result in an energy release which could threaten containment integrity.

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Hydrogen transport before chemical reaction, deflagrations or detonations, can be significantly affected by several mechanisms that mix the gases. These mechanisms include the momentum of the source flows, forced convection, intercompartment flow driven by pressure differences, natural convection, and molecular and turbulent diffusion. When hydrogen and steam source flows are introduced into the containment as jets, a well mixed source compartment would result, but gradients in the gas concentrations between the source compartment and adjacent compartments would occur. The use of engineered safety features (ESFs), fan coolers and sprays, would also tend to promote mixing in some locations in containment, particularly in the dome region.

This analysis focused on risk significant scenarios, small break loss-of-coolant accident and loss of offsite power, which had strong source injection flows. The steam and hydrogen flows into containment were calculated with the MARCH 2 computer code [1]. (The calculations were actually performed with the MARCON 2.0P computer code which links MARCH 2 with CORCON MOD 2. However, since these were arrested sequences, only the MARCH 2 models were invoked in the calculations).

The steam flows calculated by MARCH 2 are primarily dependent on the primary system pressure and the elevation of the flow junction, either a relief valve or break, relative to the height of liquid in the primary system. Only water flow occurs when the break or relief valve is below the collapsed liquid level and only gas flow occurs when it is above the water level. Two-phase (liquid-steam) flow is not modeled. The hydrogen source terms are primarily dependent on the hydrogen generation rate from the zirconium-steam reaction and the leak rate from the primary system into containment. The calculated steam and hydrogen flow rates are proportional to their mass fractions in the vessel. Therefore, while the steam and hydrogen flow rates used in this study are considered to be representative of the expected flow rates for these scenarios, when more advanced mechanistic models for core melt phenomena are developed and more accurate steam and hydrogen flow rates are available, this type of study should be expanded. The HECTR containment analysis computer code [2] was selected for this analysis because of its relatively fast-running capability for multicompartment analysis; however, some limitations of the models in HECTR to perform this type of analysis were acknowledged at the initiation of this study. Specifically, HECTR is a lumped parameter code. Flows between compartments in a HECTR model are pressure and buoyancy driven with inertial and resistance terms included. Gases in each compartment are instantaneously mixed. Simplified conservation equations are solved in HECTR to evaluate compartment and junction parameters during a transient calculation. HECTR does not model molecular diffusion, turbulent diffusion, momentum flux or compressible flow. HECTR does have models for the ESFs. The overall limitations of HECTR were not considered significant for the scenarios selected in this study.

## Steam and Hydrogen Source Terms

The steam and hydrogen source terms calculated by MARCON 2.0P for two representative risk significant scenarios were used in this analysis. The two scenarios selected were a small break loss-of-coolant accident ($S_2D$) and a total failure of AC and DC power with a loss of auxiliary feedwater to the steam generators (TMLB'). For the small break loss-of-coolant sequence, a two-inch diameter break in the primary coolant system at the initiation of the break was assumed. Two elevations for the break were used. One elevation was 10.7 ft. above the core inlet, which corresponds to the relative elevation of the pump suction piping, and the second elevation was 22.3 ft the core inlet, which corresponds to the relative elevation of the cold leg piping. However, since the primary system is modeled as a single volume in MARCH 2, the 10.7 foot elevation would not simulate a pump suction line break but a rupture of the primary vessel.

Two MARCON calculations for the steam and hydrogen flows into containment were also performed for the TMLB' sequence. The difference in the calculations was the modeling of the PORV. For one calculation the PORV was modeled to be stuck open after 42 minutes, and for the second calculation the PORV was modeled to cycle between its opening and closing set points. The flows for the first 42 minutes were identical for both TMLB' calculations and were obtained from a RELAP5 calculation of this sequence [3] which modeled the cycling of the PORV. The MARCH 2 models were initialized to the conditions in the RELAP5 calculation at 42 minutes and then each MARCH 2 calculation was begun.

For these degraded core scenarios, it was assumed that Emergency Core Coolant (ECC) injection was unavailable for a time period long enough for 75% of the clad zirconium to oxidize, but was available to arrest the sequence at 75% metal-water reaction. To arrest a sequence at 75% clad oxidation required performing several MARCH 2 runs, varying the time of initiation of high pressure injection (HPI) to calculate 75% clad oxidation.

The calculated water flow rate into containment for the $S_2D$ sequence when the break elevation was relatively low is shown in Figure 1. The flow started at about 25,000 lbs/min at the initiation of the transient and decreased to 14,000 lbs/min as the system depressurized. The flow remained at 14,000 lbs/min until the break uncovered at 30 minutes and the flow changed from liquid to steam. The gas flow from the primary system continued to decrease the system pressure. At 84 minutes there was a step increase in the water flow rate when the system pressure decreased below the pressure of the core flood tanks (CFTs) and their liquid was injected into the primary system. The core flood tanks were empty by 89 minutes and the water flow to containment again decreased. The HPI pumps were started at 105 minutes to arrest the clad oxidation. The large cycling of the water flow after 105 minutes was due to the calculated covering and uncovering of the break. When the break was covered, liquid flowed out of the break and the mass flow rate was relatively high, and when the break was uncovered gases flowed out the break and the mass flow rate was relatively low.

The calculated hydrogen mass flow rate into containment for the relatively low break elevation is shown in Figure 2. Hydrogen flow was initiated at 50 minutes and a peak flow of 94 lbs/min was calculated at 84 minutes. Shortly after this time, hydrogen flow was stopped due to quenching of the core from the emptying of the core flood tanks and the flow into containment changed from gas flow to liquid flow, due to the covering of the break. The break quickly uncovered again and hydrogen flowed into the containment, but at a significantly lower rate than before the core flood tanks emptied. Similar to the water flow rate, after 105 minutes when the HPI pumps were initiated to arrest the clad oxidation, the hydrogen mass flow rate also oscillated due to the covering and uncovering of the break elevation.

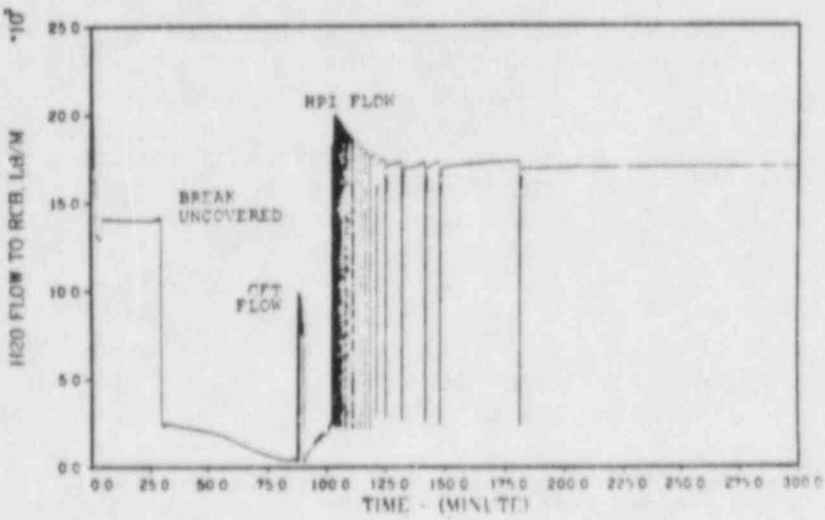


Figure 1 Calculated Water Mass Flow Rate for a Small Break LOCA with a low Break Elevation

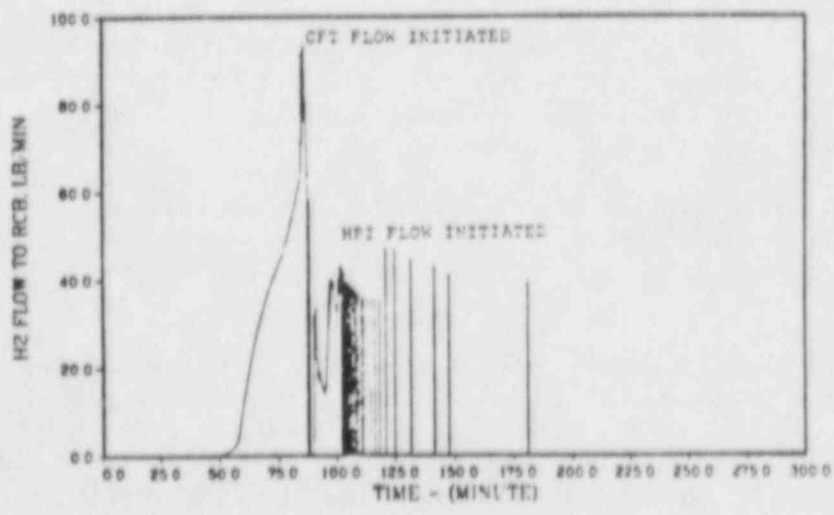


Figure 2 Calculated Hydrogen Mass Flow Rates for a Small Break LOCA with a low Break Elevation

For the TMLB' sequences, the peak water flow rates were somewhat higher (up to 41,000 lbs/min) for the first 40 minutes of the transient due to the cycling of the flow out of the safety valves. The HPI pumps were started at 84 minutes and water flow rates (approximately 10,000 lbs/min) were slightly lower than in the $S_2D$ calculations. Similar to the $S_2D$ calcuations, the hydrogen flow started at about 50 minutes. Table 1 summarizes the calculated peak hydrogen flow rates into containment and the corresponding steam flow rates. The times of initiation of HPI flow are also listed. The clad oxidation was terminated at 75% shortly after the initiation of HPI flow; therefore, the time of HPI flow initiation is indicative of the length of time hydrogen was flowing into the containment.

TABLE 1

Peak Hydrogen and Corresponding Steam Flow Rates

| Sequence | Peak Hydrogen Mass Flow (lb/min) | Steam Flow at Peak Hydrogen Flow (lb/min) | Peak Hydrogen Mole Fraction | HPI Initiated (min) |
|---|---|---|---|---|
| $S_2D$ (Low Break Elevation) | 94 | 120 | 0.87 | 100 |
| $S_2D$ (High Break Elevation | 65<br>48 | 140<br>140 | 0.81<br>0.75 | 198 |
| TMLB' (Stuck Open PORV) | 95 | 423 | 0.67 | 85 |
| TMLB' | 114 | 542 | 0.65 | 90 |

## Containment Models

The large, dry containment modeled for this analysis was a steel-lined concrete cylinder with an elliptic dome roof supported by a concrete foundation ring. The net free volume of the containment was 3,389,000 cubic feet. The cylindrical wall has an inside diameter of 135 feet, a thickness of 3.5 feet and a height of 227 feet. The cylindrical inner wall is lined with 1/4 inch steel. Figures 3 and 4 show vertical cross sections of the containment that is modeled and illustrate the relative elevations of the main floors and equipment in the containment. The walls (D rings) that separate the steam generators and the reactor are also indicated in Figures 4 and 5. Also illustrated in these figures is that about 70% of the net free volume is above the fourth floor (Elevation 700) and is essentially open space. The major openings between floors outside of the D rings are stairways, an elevator shaft, equipment hatches, floor piping penetrations, and a seismic gap (a 1 ft. circumferential gap between the floors and the containment inner wall).

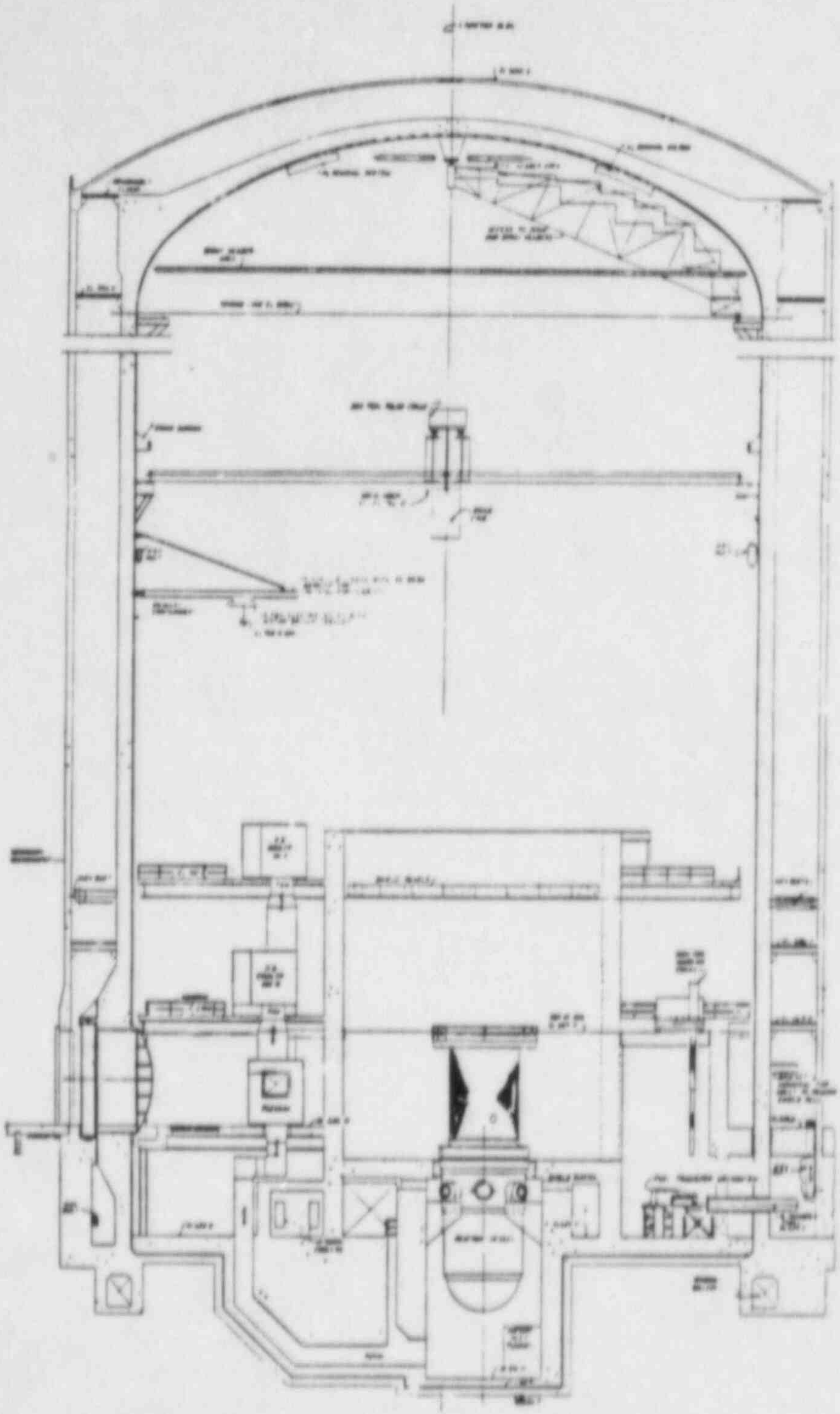

Figure 3 Large, Dry Containment Vertical Cross Section

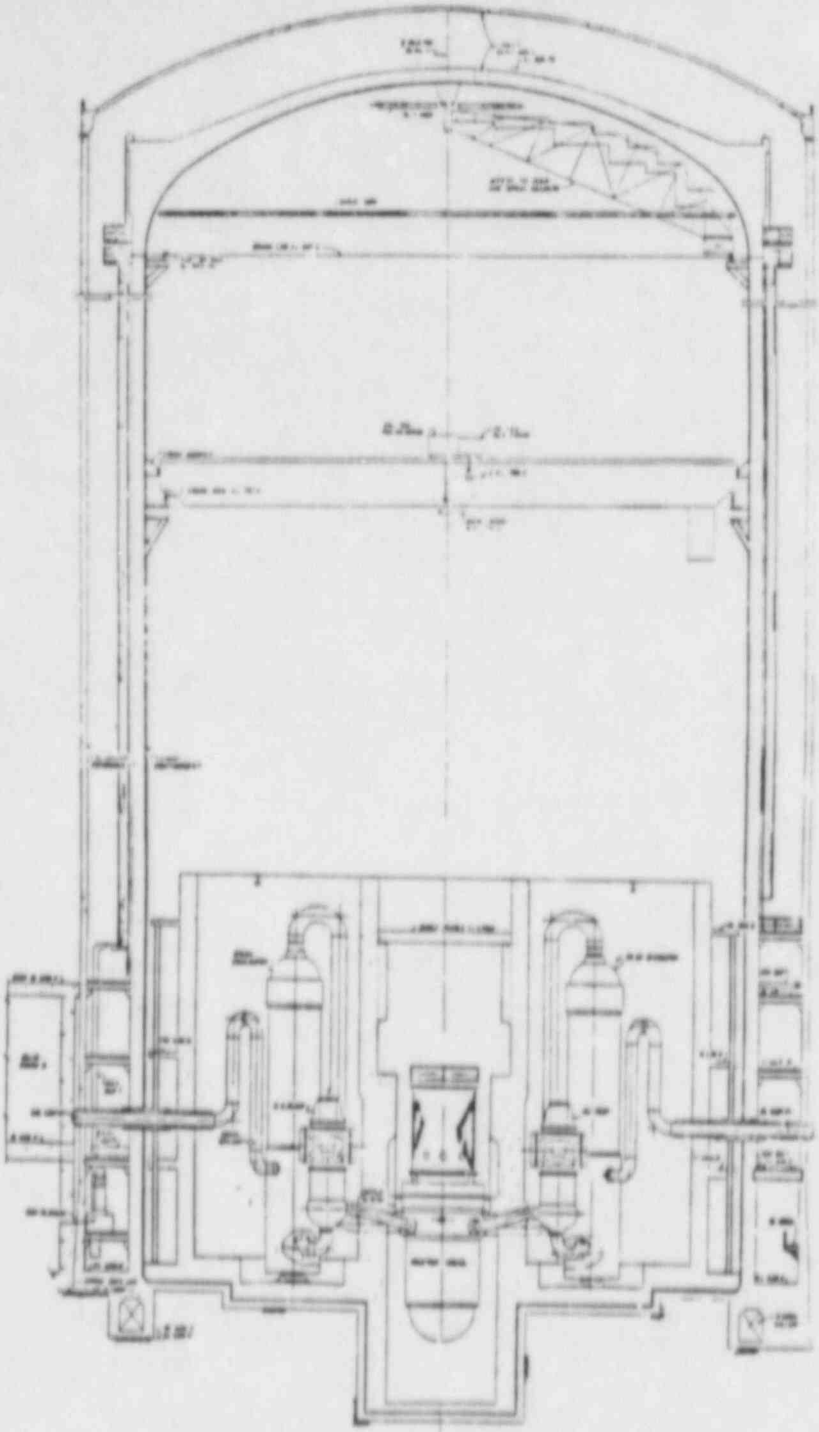

Figure 4 Large, Dry Containment Vertical Section Cut

The containment was modeled with compartments that were generally selected to coincide with physical boundaries such as walls and floors. Some compartments however, were not completely bounded by physical stuctures. Flow junctions at compartment boundaries were generally based on physical boundaries such as doors, hatches, equipment, walls, or penetrations. Several pieces of equipment are large, and when placed near walls, form flow passages. Some flow junctions were artificial, being arbitrarily placed through open spaces with no bounding walls to guide the flows. Figure 5 illustrates how compartments were selected for this study. The numbers shown on this figure are compartment numbers. (Note the relative locations of compartments 6 and 10 in SG1A which were the location of the breaks for the $S_2D$ calculations compared to compartment 22 which was the location of the source for the TMLB' calculations).

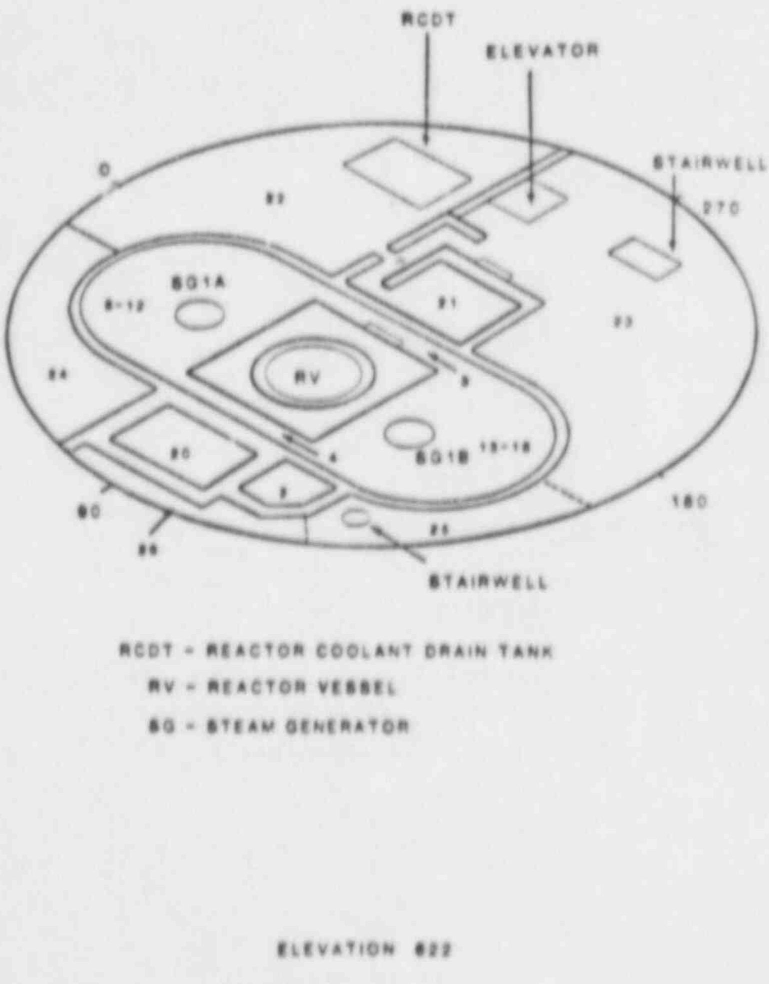


Figure 5 Representative Compartmentalization of Containment

Four basic heat transfer surfaces were modeled by HECTR: 1) the inner steel liner of the containment walls, 2) concrete surfaces of the internal structures, 3) equipment surfaces, and 4) sump (water) surfaces.

The containment ESFs, reactor building cooler (RBC) system and the reactor building spray (RBS) system were also modeled. The RBC system consisted of three air circulation units and the RBS system consisted of two independently operated spray systems. The effects of operational and nonoperational ESFs upon the containment atmosphere were calculated for both the $S_2D$ and TMLB' scenarios. For TMLB', ESF operational status was resumed when ECC injection was restored.

Four basic HECTR models of the containment were used in this analysis. The primary difference was in the number of compartments, flow junctions and heat transfer surfaces. The models were varied to investigate the effects of compartmentalization on the calculated results and on the computer run time. The models used are summarized in Table 2.

TABLE 2
Summary of HECTR Models

| Model Identification | No. of Compartments | No. of Junctions | No. of Heat Structures | Sequence |
|---|---|---|---|---|
| Base | 42 | 130 | 125 | $S_2D$ |
| A | 35 | 95 | 112 | $S_2D$ TMLB' |
| B | 29 | 75 | 94 | TMLB' |
| C | 27 | 70 | 86 | TMLB' |

## Results

As previously discussed, two series of calculations for an $S_2D$ sequence in which the break elevation was varied and two series of calculations for a TMLB' sequence in which the flow from the PORV was varied were performed. The results presented in this paper will focus on discussing the $S_2D$ calculation with a break elevation low in the vessel and a TMLB' calculation with a stuck open PORV. (Detailed results for all of the calculations will be available in a future report.) The results for the sequences selected are also representative of the other sequences calculated.

### Small Break $S_2D$

For the relatively low small break elevation, the most detailed containment model was used (42 compartments). The water and hydrogen flows, previously discussed and shown in Figures 1 and 2, were input as sources for the HECTR calculation. The pressure response of the containment building is shown in Figure 6. The calculated pressure response was dominated by the water/steam mass flow rates calculated by MARCH 2. The containment pressure increased continuously until the break elevation was uncovered at 1800 s at which time the flow into containment from the primary decreased substantially (as previously illustrated in Figure 1). This change in flow resulted in a decrease in the calculated pressure. A second increase in pressure was calculated at 5200 s when the primary pressure dropped below the pressure of the CFTs allowing their water to be injected into the reactor vessel. The pressure increased again at 5700 s when the core uncovered and steam once again was released from the break location. The RBS system activation pressure (20 psi increase in pressure) was never reached during the calculation.

The calculated water, nitrogen, oxygen, and hydrogen mole fractions in the containment dome are shown in Figure 7. The calculated mole fraction of steam also indicated a strong dependence on the water flow rate from the primary system. Figure 7 shows that hydrogen was first present in containment at 3700 s and that the concentration increased until the core was quenched and the hydrogen generation temporarily stopped by the water injected into the vessel from the CFTs at 5200 s. The hydrogen mole fraction in the dome was representative of most compartments in containment (except for the reactor cavity) that were not in direct contact with the source compartment. The difference in the hydrogen mole fractions was less than 0.01 throughout the containment at the end of the calculation.

The calculated gas mole fractions in the source compartment are shown in Figure 8. The source compartment is the compartment into which hydrogen and steam are released through the primary break. As expected, both the hydrogen and steam mole fractions reflected the relative magnitudes of the leak flow rates of steam and hydrogen. Following the uncovering of the break, the steam mole fraction in the source compartment decreased (due to decreasing steam flow rate from the primary system) until the core flood tanks emptied into the vessel. Water entering the primary system from the CFTs stopped the generation of hydrogen and forced part of the hydrogen and steam vapor in the primary system out of the vessel, temporarily increasing the flow of hydrogen and steam out of the break. This increase in flow resulted in the highest calculated local hydrogen concentration of 11.3% for this sequence. The steam mole fraction at this time was 14%. The hydrogen concentration dropped from its peak value as the hydrogen source flow decreased due to the quenching of the core and covering of the break by the CFT. The break was uncovered by 5400 s. The gradual increase in hydrogen concentration from 5700 to 6100 s was due to the hydrogen flow from the primary system and hydrogen flowing into the break compartment from the containment air circulation. The oscillations in the calculation of the steam and hydrogen concentrations after 6100 s were caused by the covering and uncovering of the break elevation in the MARCON calculation. When the break was covered, the flow out of the break was liquid while the flow was vapor when the break was uncovered.

The calculated mole fractions of the gases in a compartment adjacent to the source compartment are shown in Figure 9. The overall trends were similar to the source compartment with a peak hydrogen mole fraction approximately 0.02 lower than in the source compartment. The hydrogen mole fractions in all compartments in the model were nearly equal at about 0.09 by the end of the calculation indicating relatively complete mixing throughout the containment. The source compartment and its adjacent compartment are located in SG1A of Figure 5.

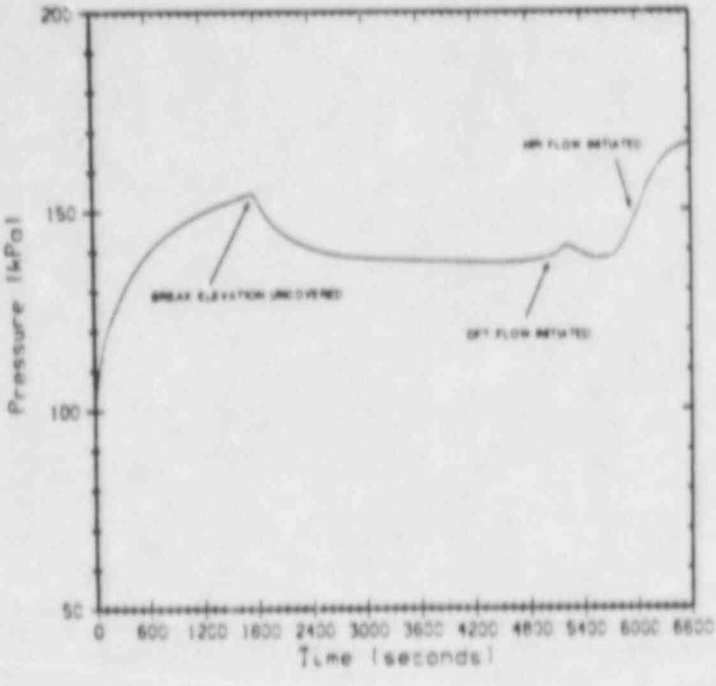


Figure 6  Small Break LOCA Containment Pressure Response

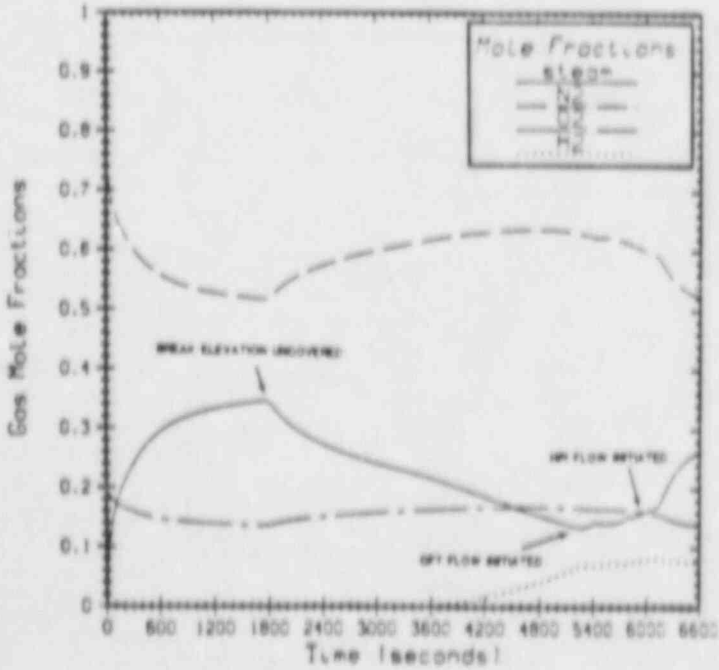


Figure 7  Small Break LOCA Dome Compartment Gas Mole Fractions

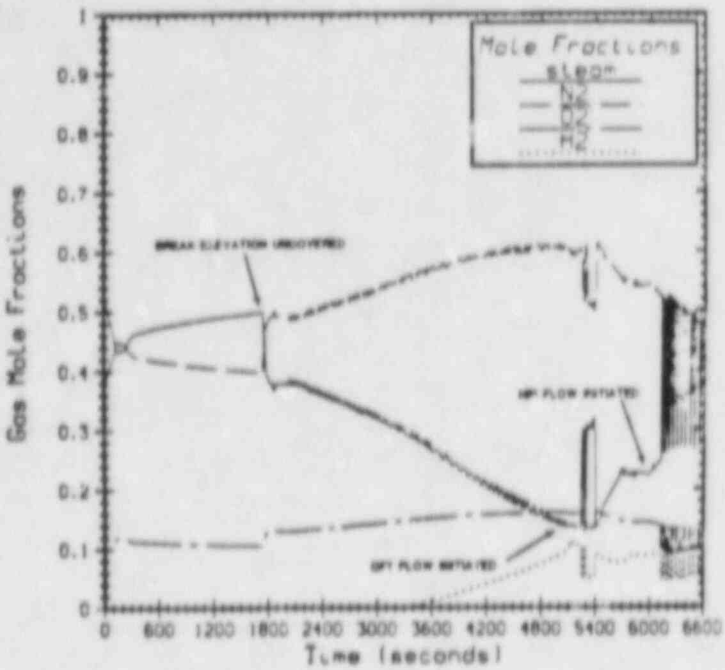


Figure 8  Small Break LOCA Source Compartment Gas Mole Fractions

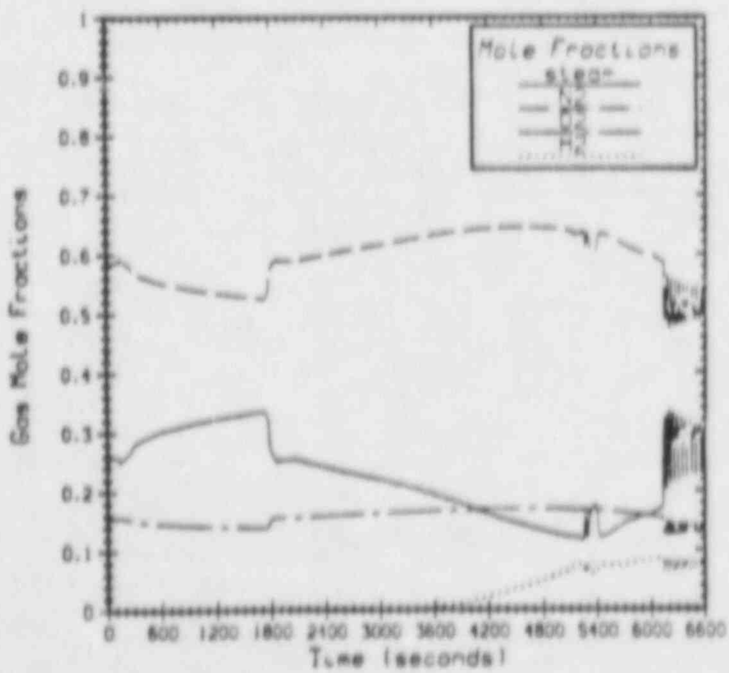


Figure 9  Small Break LOCA Compartment 8 Gas Mole Fractions

The steam mole fraction in the reactor cavity was significantly different from the other compartments as indicated in Figure 10. The steam concentration was relatively low at the initiation of the transient and remained low during the transient due to steam condensation in the reactor cavity resulting from the introduction of cool air from the reactor building cooler. The final hydrogen concentration in the reactor cavity was about 11% which was 2% higher than the rest of containment.

The dominant flow directions in containment during the release of water and hydrogen in the transient are shown in Figure 11. The flow directions were affected by three factors: the hot steam generator surfaces, the reactor building cooler flows, and the leak rate. The D-ring walls around the steam generator near the break location contained the flow and forced the hot gases up through all levels of the steam generator into the dome region. Part of the gas leaving the steam generator compartments near the break recirculated into the dome region while the rest flowed down through the compartments of the other steam generator levels, the refueling canal, and the floors. The fluid entering the lowest elevation then flowed into the bottom of the steam generator compartments near the break where it was entrained with the leak source flow and pushed upward through the steam generator compartments again.

The results of the seven small break LOCA calculations are summarized in Table 3. Comparing Cases 1A and 1B shows that decreasing the volume of the compartment (compartment 6 versus compartment 10) into which the steam and hydrogen flows were injected increased the peak hydrogen concentration. Run 1C shows that when the break was in the reactor cavity, high concentrations of hydrogen were calculated; however, there was no oxygen present, and the compartment would be inerted. Comparing Cases 1A, 1D, and 1E shows that the operation of the RBC and RBS decreased the peak steam concentration, but had a small effect on the peak hydrogen concentrations.

TABLE 3
Summary of Small Break Calculations

| Case | Break Elevation | Containment Model | Break Compartment Volume ($Ft^3$) | ESF On | Peak Hydrogen Mole Fraction | Corresponding Steam Mole Fraction |
|---|---|---|---|---|---|---|
| 1A | Low | Base | 8793 | RBC | 0.11 | 0.14 |
| 1B | Low | Base | 4344 | RBC | 0.14 | 0.17 |
| 1C | Low | A | 26768 | RBC | 0.62(1) | 0.38 |
| 1D | Low | Base | 8793 | None | 0.10 | 0.23 |
| 1E | Low | A | 8793 | RBS(2) | 0.11 | 0.17 |
| 2A | High | A | 4344 | RBC | 0.11/.12 | 0.16/.15 |
| 2B(3) | High | A | 4344 | RBC | 0.13/.15 | 0.15/.14 |
| 2C | High | A | 2225 | RBC | 0.26/.24 | 0.30/.25 |

(1) No oxygen present
(2) Initiated at 1400s
(3) Artificially increased hydrogen flow by 25%

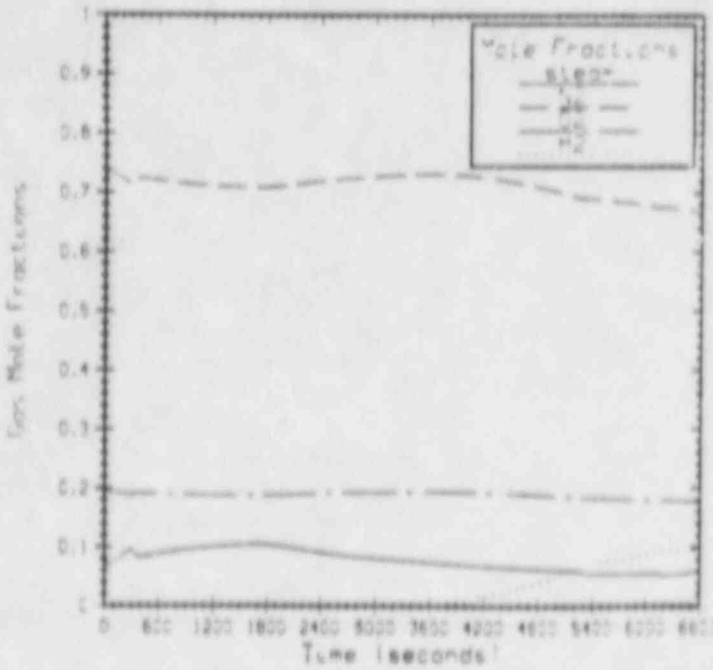


Figure 10 Small Break LOCA Reactor Cavity Gas Mole Fractions

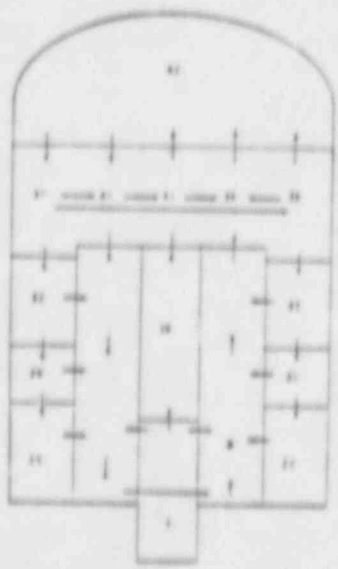

Figure 11 Small Break LOCA Containment Circulation Flows

For the high break elevation, two peak values for the steam and hydrogen are shown because there were two distinct peaks in the hydrogen flow rate. For Case 2B the hydrogen flow was artificially increased by 25% to investigate the effect of the source flows on the calculated peak concentrations. As expected, the peak hydrogen concentration was higher for Case 2B than Case 2A by about 25%. For Case 2C the source flows were injected into a compartment modeling the steam generator tunnel, which was one of the smallest compartments in the model, and gas concentrations were calculated that were within the detonable limits of Reference 4. A break in the coolant line from the CFT to the reactor vessel could result in hydrogen and steam release into the steam generator tunnel.

### Loss of Offsite Power - TMLB' (Stuck Open PORV)

This TMLB' scenario with the PORV stuck open after 42 minutes used containment model A (see Table 2). (The source compartment was Compartment 22 as shown in Figure 5.) The calculated containment pressure response is shown in Figure 12 and indicates that the water/steam released from the primary dominated the pressure response. The containment pressure rapidly increased to a peak value at 1600 s and oscillated about the peak value due to cycling on and off of the safety relief valves. The pressure decreased during the period from 2000 to 5000 s as the water/steam mass flow rate from the primary system decreased and due to condensation on the relatively cool containment walls. The pressure increased at 5000 s when the HPI flow into the primary was started, allowing more water/steam to leave through the open PORV.

The calculated gas mole fractions in the dome are presented in Figure 13 and are representative of most compartments in containment that were not directly connected to the source compartment. A large flow of water from the primary system was calculated from 1000-2000 s when the safety relief valves cycled open and shut. The calculated hydrogen concentrations began increasing in containment at 3500 s. The hydrogen concentration leveled out at 6900 s. At 6900 s the hydrogen was uniformly mixed throughout containment and varied by less than a mole fraction of 0.01 in compartments removed from the break location at the end of the scenario. Final steam mole fractions in compartments not connected to the source compartment showed less than a variation of 0.03 throughout containment.

The calculated gas mole fractions in the source compartment are shown in Figure 14. The source compartment filled completely with steam while the safety relief valves were cycling open and shut. Hydrogen began to flow into the source compartment after the safety relief valves had cycled and while the PORV was stuck open; at the same time, the steam mole fraction began to drop as the steam flow rate from the primary system decreased. Both the hydrogen and steam mole fractions began to increase rapidly at 5000 s when HPI flow into the vessel was initiated. Water entering the vessel displaced the existing hydrogen and steam in the primary system and forced it out through the PORV. A maximum hydrogen mole fraction of 0.15 with a corresponding steam mole fraction of 0.42 was calculated at 5100 s. The hydrogen concentration dropped during the period from 5100 to 6600 s when the core was quenched by the HPI flow and the hydrogen generation stopped. The hydrogen concentration dropped rapidly at 6600 s when water in the vessel reached the elevation of the safety relief valves; at this point, water released through the PORV increased rapidly.

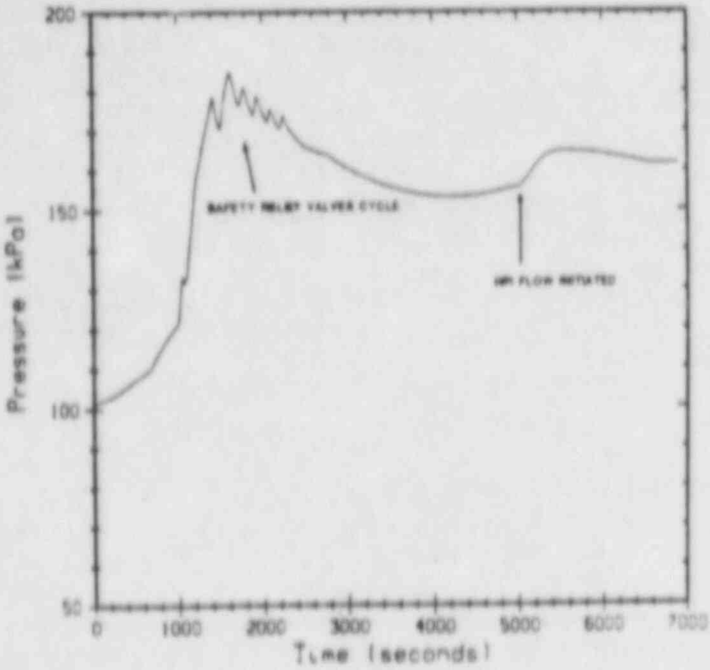


Figure 12 Loss of Offsite Power Containment Pressure Response

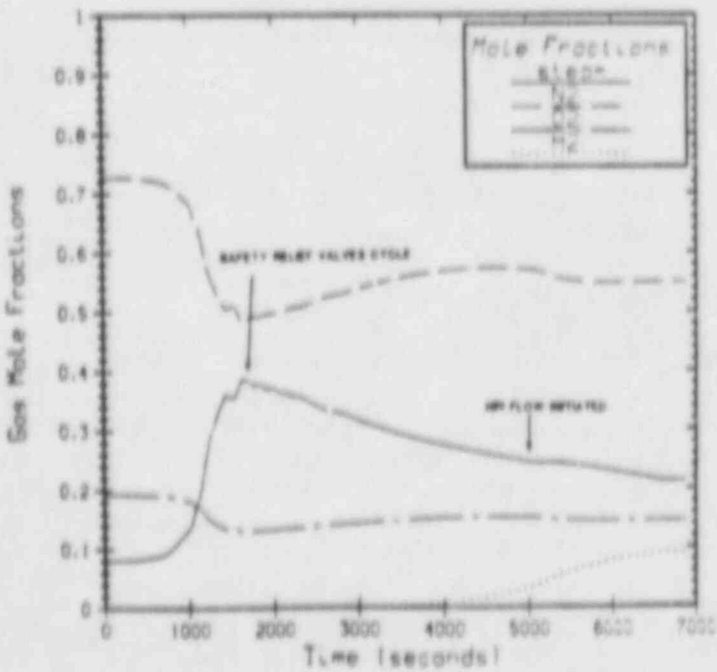


Figure 13 Loss of Offsite Power Dome Compartment Gas Mole Fractions

The predominant containment circulation flows for the TMLB' sequence were dominated by the primary steam leakage rate. The circulation flows during the period of hydrogen release are shown in Figure 15. Steam and hydrogen entering Compartment 22 flowed upward through the floor penetrations of the lower elevations which are directly above Compartment 22. Flow also was calculated to be upward through the steam generator compartments into the dome primarily due to the hot steam generator surfaces heating the gases in these compartments.

The results of the seven loss of offsite power calculations are summarized in Table 4. Cases 3A through 3F investigated the effects of compartmentalization on the calculated gas concentrations. The volume containing the location of the steam and hydrogen injection flows was divided into a larger number of compartments. The highest hydrogen concentration was calculated when the volume was divided azimuthally into four equal compartments and the injection was into a relatively enclosed volume. The case 3E source compartment was not as enclosed as the Case 3B and 3D source compartments resulting in higher volumetric flow rates through the Case 3E source compartment and lower peak concentrations. Comparing Cases 3A and 4 shows that modeling the cycling of the PORV did not affect the peak hydrogen concentrations. For the TMLB' sequences analyzed the peak hydrogen concentrations were always calculated in the source compartment and the calculated steam mole fraction at the peak hydrogen concentration was above 42%, which indicates that compartment would be steam inerted with respect to detonation limits.

TABLE 4
Summary of Loss-of-Offsite Power Calculations

| Case | PORV(1) | Containment Model | Break Compartment Volume ($Ft^3$) | Peak Hydrogen Mole Fraction | Corresponding Steam Mole Fraction |
|---|---|---|---|---|---|
| 3A | Stuck Open | A | 21474 | 0.15 | 0.42 |
| 3B | Stuck Open | B(2) | 5369 | 0.20 | 0.52 |
| 3C | Stuck Open | C(3) | 10737 | 0.18 | 0.47 |
| 3D | Stuck Open | B(4) | 5369 | 0.22 | 0.56 |
| 3E | Stuck Open | B(4) | 5369 | 0.15 | 0.43 |
| 3F | Stuck Open | C(5) | 10737 | 0.15 | 0.43 |
| 4 | Cycles | A | 21474 | 0.15 | 0.48 |

(1) PORV cycles before 42 min.
(2) Base Compartment 22 divided azimuthally and axially into 4 equal compartments.
(3) Base Compartment 22 divided azimuthally into 2 equal compartments.
(4) Base Compartment 22 divided azimuthally into 4 equal compartments.
(5) Base Compartment 22 divided vertically into 2 equal compartments.

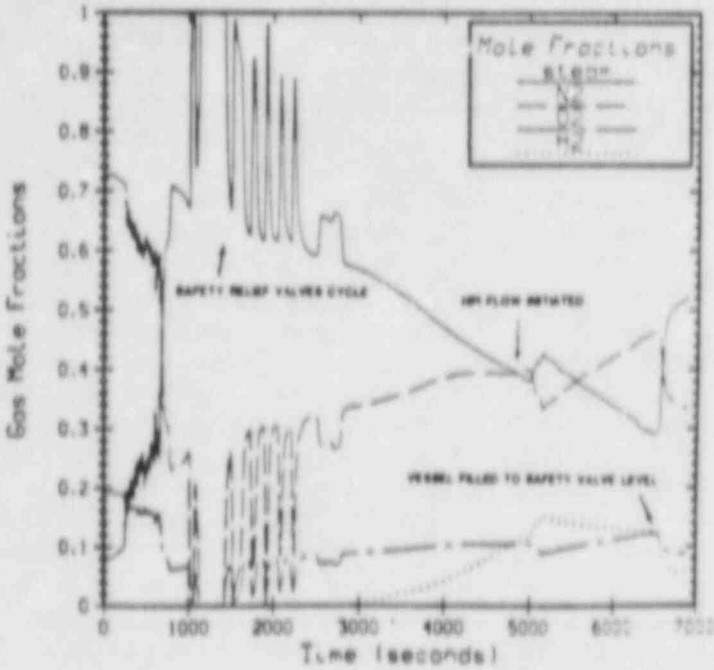


Figure 14 Loss of Offsite Power Source Compartment Gas Mole Fractions

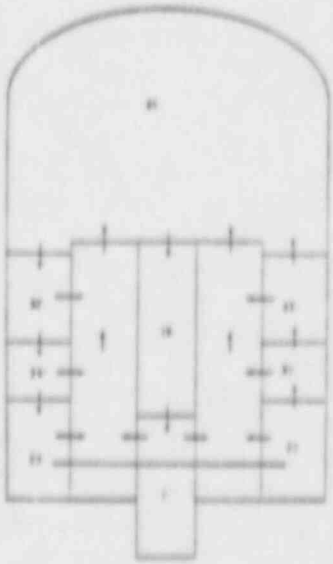

Figure 15 Loss of Offsite Power Containment Circulation Flows

## Summary and Conclusions

The conclusions from this analysis are based on results from HECTR calculations of gas transport. The HECTR computer code was developed to analyze nuclear reactor accidents involving the transport and combustion of hydrogen; however, since HECTR was developed based on lumped-parameter models, the interpretation of these results must be viewed within the limitations of the basic modeling assumptions. The potential effects of these modeling assumptions are being investigated by comparing results from this analysis with results from a computer code based on finite difference models.

The results from the HECTR calculations showed that the source flows were the dominant factors in establishing the circulation flow in containment. As long as there were source flows, the buoyancy effects on the gases due to heat transfer from structures were secondary factors. The calculated circulation flows caused relatively good mixing of the gases throughout the containment.

The highest concentrations of hydrogen were always calculated in the source compartment. For the containment geometry and the scenarios analyzed, the calculated local hydrogen concentration outside the source compartment showed no significant increases above the overall average. The size of the source compartment affected the calculated peak hydrogen concentration.

Calculations with steam and hydrogen source flows which are representative of a small break loss-of-coolant accident showed that at the peak hydrogen concentrations the compartments were not steam inerted. Concentrations that were near the assumed detonation limits were calculated. In fact, when the source flows were injected into a compartment modeling a steam generator tunnel, which was the smallest compartment in the model, a detonable concentration was calculated. Further analysis is required to determine the likelihood of this event, the potential for detonation in the steam generator tunnel, and its effects on containment or safety related equipment. Hydrogen flammability and detonability limits used in this study were based upon studies by Shapiro and Moffette [4].

Calculations with steam and hydrogen source flows which are representative of loss of AC and DC power and turbine driven auxiliary feed water flow showed that at the peak hydrogen concentrations the steam concentration was greater than 42%. At this steam concentration, the compartment would be outside the detonation limit, but could be within the flammability limit.

There are potentially significant modelling uncertainties in the computer codes used in this analysis. The MARCH2 models in the MARCON code that generate the steam and hydrogen source flows probably have a large uncertainty. As more mechanistic core melt codes are developed (MELPROG, MELCOR, SCDAP/RELAP5), the typicality of the steam and hydrogen source flows should be re-evaluated. The effects of the models in the HECTR code that affect the hydrogen transport results should also be evaluated by comparing these results to results from a multi-dimensional finite difference containment code.

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SAND85-1824C

# Pressure-Temperature Response in an Ice-Condenser Containment for Selected Accidents*

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## ABSTRACT

The effects of recirculation loops in the ice bed region of an ice-condenser containment are described in this paper. HECTR, a lumped-parameter computer code, was used for this investigation. Although best-estimate calculations are not possible, sensitivity calculations can be performed to bound the effects and provide qualitative insights. Results of sensitivity calculations, as well as results calculated for small break scenarios and a transient with failure of all AC power, are presented. The calculations indicate that the formation of recirculation loops in the ice bed does not prevent detonable mixtures from forming in regions of the ice bed when the containment fans are not operating. In addition, preferential melting of a region of the ice bed was predicted in all of the scenarios, even those in which the air return fans were operating.

## INTRODUCTION

An area of focus of the Severe Accident Sequence Analysis (SASA) program has been the pressure-temperature response of ice-condenser containments to severe accidents, particularly those involving the release and combustion of significant quantities of hydrogen. This paper addresses some of the specific phenomenological questions that have been raised in these previous SASA analyses [1,2].

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One of the areas investigated in references 1 and 2 was the adequacy of using igniters to reduce the threat posed by hydrogen combustion during severe accidents. Igniters have been installed in most regions of ice-condenser containments in an attempt to provide controlled burning of hydrogen at relatively low concentrations. However, igniters are not present in the ice bed region of the ice condenser. Thus, controlled burning of hydrogen at low concentrations can not be guaranteed in this region for all scenarios, even if the igniters are operating in other regions of containment. Additionally, the igniters are AC-powered and will not be available during accidents involving the loss of AC power.

In references 1 and 2, detonable mixtures of hydrogen were predicted to occur in the ice bed during scenarios in which the igniters were operating, but the containment air return fans were not. In these scenarios, burning was prevented in the lower compartment by steam-inerting (fan failure prevented return air flow from the upper compartment). As the gases flowed from the lower compartment to the upper compartment through the ice bed, the hydrogen concentration continually increased due to condensation of steam from the mixture. Without the large driving force provided by the fans, detonable mixtures were predicted to occur near the top of the ice bed. These detonable mixtures were reached before a combustible level was reached in the upper plenum (which contained igniters). Therefore, the igniters were <u>not</u> predicted to be effective in preventing the accumulation of detonable mixtures.

Station blackout sequences involving the loss of both the igniters and the air return fans were also examined in references 1 and 2. In these cases it is difficult to predict how and when the hydrogen will burn, if indeed it burns at all. To better understand the potential risk from these sequences, it is useful to examine the gas concentrations in various regions of containment as a function of time. This information provides insights about the potential for various combustion processes and the times during which those processes are possible. The previous analyses indicated that the potential exists for detonable mixtures to form during substantial portions of these accidents.

The previous calculations discussed above were performed with the MARCH [3], MARCON [4], and HECTR [5] computer codes. MARCH and MARCON were used to predict the primary system response and HECTR was used to predict the containment response. A nine-volume containment model was employed, with the ice bed region consisting of a single vertical stack of 4 compartments. The ice bed was not subdivided circumferentially; therefore, recirculation loops in the ice bed, driven by natural convection, could not be predicted. It has been suggested that these loops could result in enhanced mixing within the ice bed (as well as mixing of the ice bed and upper plenum regions), thereby reducing the tendency to form and maintain detonable mixtures in the ice bed region.

To investigate the effect of recirculation loops on the concentration of hydrogen within the ice bed, we have modified HECTR such that the ice bed can be divided circumferentially as well as axially. This allows recirculation loops within the ice bed region to be modeled. This also allows us to examine the potential for non-uniform melting of the ice bed. It should be emphasized that in this analysis, we are not addressing the probability that a detonation would occur, but rather the potential for forming a detonable mixture.

## MODEL DESCRIPTION

For this analysis, we used a finer nodalization (40 volumes) than had been used in the previous analyses (9 volumes). The nodalization used is shown in Figures 1 through 4 and the flow paths are summarized in Table 1. Figure 1 presents a schematic view of the containment, identifying the relative locations of the compartments. The upper compartment was divided into four HECTR compartments: the refueling space, the region above the refueling floor but below the upper dome, and two upper dome compartments. Figure 2 shows the nodalization of the lower compartment and annular regions. The lower compartment was divided into nine compartments: the pressurizer doghouse, the upper reactor cavity, four compartments for the four steam generator doghouses, and three compartments for the remaining region of the lower compartment. The annular (dead-ended) region surrounding the lower compartment was divided into three compartments. Figures 3 and 4 show the nodalization of the ice condenser region. The region was divided circumferentially into four columns and the ice bed region was divided axially into four layers. Thus, the entire region is represented by 24 compartments (16 in the ice bed and 4 each in the upper and lower plena). Figure 3 looks down on the ice condenser region and shows the circumferential division while Figure 4 shows the "unwrapped" side view. Steam and hydrogen were injected into compartment 8 in all of the calculations reported in this paper.

## RESULTS

Numerous calculations were performed using the 40 volume deck described above. First, calculations were performed to examine the effect of flow loss coefficients, heat transfer and condensation rates, and steam injection rates on the calculated flow patterns in the ice bed. After examining the results of these sensitivity calculations, additional calculations were performed for two accident scenarios that had previously been analyzed in References 1 and 2. These cases were recalculated for this analysis to examine the potential for detonable mixtures and non-uniform melting when recirculation loops were possible in the ice bed region.

## Sensitivity Calculations

The parameter ranges examined for the sensitivity calculations are summarized in Table 2. The flow loss coefficients in the ice bed and the steam injection rate into the lower compartment were varied over a wide range. In addition, cases were run with the default HECTR heat and mass transfer correlations multiplied by 10 and then divided by 10.

For these sensitivity calculations, HECTR usually predicted the formation of recirculation loops in the ice bed. The flow pattern consisted of upward flow through two of the columns and downward flow through the remaining two columns. Volumetric flow rates for four junctions in the ice condenser are listed in Table 2 for each of the sensitivity calculations to indicate the size and location of the recirculation loops. Only cases with high steam injection rates were predicted to have upward flow through all four columns of the ice bed, rather than forming recirculation loops.

The results of these sensitivity calculations indicate that recirculation loops are likely to form in the ice bed for most scenarios. Once formed, the recirculation loops will tend to be sustained by buoyancy forces. The likelihood of forming recirculation loops is increased further if potential asymmetries are considered. The sensitivity calculations we performed did not consider variations in the performance of the ice condenser doors or the effects of varying the steam source location. Such considerations would introduce additional asymmetries which would affect the results and could further increase the tendency to form recirculation loops.

The flow pattern in the ice bed will affect both the gas concentrations in the ice bed and the pattern of ice melting. The mass of ice melted (per column) in each of the sensitivity calculations is listed in Table 3. In all cases, the largest amount of ice melting occurred in the center two columns. In the calculations, most of the steam was condensed from the gas as it flowed upward through the ice bed. Thus, most of the heat transfer and condensation from the gas occurred before the gas circulated back down through the outer two columns.

## TMLB' and S1HF Calculations

Three calculations were performed using the 40 volume deck for two different accident scenarios: a transient-initiated accident in which there is total failure of both AC power and steam generator feedwater (TMLB'), and a small break loss-of-coolant accident with failure of emergency core coolant and containment sprays in the recirculation mode ($S_1HF$). Fan operation is precluded by AC power failure in the TMLB' scenario. For the $S_1HF$ scenario, we performed calculations both with and without fans operating to examine the sensitivity of the results to fan operation.

The HECTR calculations predicted that recirculation loops would form in the ice bed region for all three cases (even the $S_1HF$ scenario with fans operating). For the fans-off cases, these loops caused more mixing within the ice bed region than in the HECTR calculations reported in references 1 and 2; however, detonable mixtures were still predicted (Gas mixtures were identified as potentially detonable whenever the mole fraction of hydrogen was above 14%, the mole fraction of oxygen was above 9%, and the mole fraction of steam was below 30%). Detonable mixtures did not occur in the $S_1HF$ scenario with fans operating due to the strong mixing provided by the fans. For all three calculations, the recirculation loops resulted in greater ice melting in the two columns with upward flow than in the two columns with downward flow. This would lead to earlier melt-through of a portion of the ice bed than if the melting were uniform in all four columns. Further details of these calculations are provided in the following paragraphs.

In the TMLB' scenario, igniters are not available; thus ignition is essentially a random event and can not be predicted. To examine the potential threat to containment for this scenario, we performed a calculation with ignition suppressed, then examined the results to determine which regions of containment would be flammable (or detonable) and for how long.

The steam and hydrogen sources that were used for the TMLB' calculation are shown in Figures 5 and 6. The sources are from Case Q.06 in reference 2. For this case, 49.4% zirconium oxidation was calculated to occur in-vessel. Additionally, the MARCON parameters were adjusted to produce a relatively large steam spike following vessel breach. In the TMLB' calculation, the release rate of steam and hydrogen at vessel breach is much larger than the release rates during the rest of the transient. Thus, integrated source rates were plotted in Figures 5 and 6, rather than instantaneous rates. The hydrogen release rate is predicted to be fairly low until core slump occurs at 8640 s. A second spike in the hydrogen release rate (shown as a near step change in the integrated hydrogen release plotted in Figure 6) occurs at vessel breach which is predicted to occur at 9465 s.

Our results for the TMLB' scenario are summarized in Figures 7 and 8, which show the flammability of various regions in containment following core slump and vessel breach, respectively. Results for both the 40 volume and the 9 volume models (results from reference 2) are included in the figures. In both cases, the lower regions of containment were generally not flammable throughout most of the calculation. However, combustible mixtures formed in the upper regions of containment following core slump due to steam condensation in the ice condenser. Detonable mixtures were predicted for the upper region of the ice bed and the upper plenum following core slump. These regions became inert immediately following vessel breach, but reached detonable concentrations again shortly thereafter. With the finer nodalization in the 40 volume deck, even portions of the upper containment were predicted to reach detonable concentrations following vessel breach. Since the igniters would not be operating during this scenario, it is not possible to predict whether combustion would occur during the time that these detonable mixtures are present.

The steam and hydrogen sources used to calculate approximately the first hour of the $S_1HF$ calculations are shown in Figures 9 and 10. These sources are the Case I.00 sources calculated by MARCH for the analyses reported in reference 1. This case represents a degraded core accident rather than a core melt accident. Thus, the large hydrogen releases that were predicted at core slump and at vessel breach in the TMLB' calculation are not predicted for this calculation. However, a large hydrogen release rate is predicted for this calculation shortly after restoration of emergency core cooling. Core uncovering occurs earlier in this case than in the TMLB' calculation; thus hydrogen releases to containment begin earlier (at about 3000 s). The peak hydrogen releases occur at about 3500 s.

For both $S_1HF$ calculations, ignition was assumed to occur in compartments containing igniters when the hydrogen concentration reached 7%. In the fans-off case, detonable mixtures in the ice bed region, which does not contain igniters, were predicted to form before the upper plenum, which contains igniters, reached the assumed ignition limits. In fact, detonable mixtures were predicted to occur in the ice bed by the time the upper plenum hydrogen concentration reached about 5 - 6%. Such a mixture is marginally flammable and not likely to lead to flame propagation downward into the ice condenser. The steam and hydrogen concentrations present in all compartments in the model prior to the first upper plenum burn are listed in Table 4.

Detonable mixtures were not calculated for the $S_1HF$ scenario with fans operating due to the strong mixing provided by the fans. However, recirculation loops still formed, causing greater ice melting in two of the columns than in the remaining two. The mass of ice in each of the four ice columns is shown in Figure 11. Although none of the columns had completely melted before the calculation was stopp[illegible] this preferential melting would ultimately lea[illegible] to earlier melt-through of a portion of the ice bed thar [illegible]f the melting were uniform in all four columns.

The recirculation loops also resulted in more effective cooling within the ice bed. This lowered the baseline pressures for the TMLB' and $S_1HF$ fans-on calculations from the values calculated for References 1 and 2 (For example, see Figure 12). However, this enhanced cooling will also result in an earlier meltout of the ice bed.

## SUMMARY AND CONCLUSIONS

The calculations reported in this paper indicate that recirculation loops are likely to form in the ice bed region of ice-condenser containments for most scenarios. These loops result in non-uniform ice melting, which could cause earlier melt-through of a portion of the ice bed. The loops do not appear to produce sufficient mixing to prevent the formation of detonable mixtures in scenarios in which the containment air return fans have failed. If the containment air return fans are operating, detonable mixtures are not predicted, due to the strong containment mixing produced by the fans.

## REFERENCES


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3. R. O. Wooton, P. Cybulskis, and S. F. Quayle, MARCH 2 (Meltdown Accident Response Characteristics) Code Description and User's Manual, Battelle Columbus Laboratories, NUREG/CR-3988, August 1984.

4. C. J. Shaffer, et al., "MARCON - Development and Applications to Containment Loading Calculations," Proceedings of Twelfth Water Reactor Safety Research Information Exchange Meeting, Gaithersburg, Maryland, NUREG/CP-0058, October 1984.

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Table 1. Flow Junctions

| Junction Number | Source Compart. | Receiving Compart. | Junction Number | Source Compart. | Receiving Compart. |
|---|---|---|---|---|---|
| 1 | 1 | 2 | 41** | 40 | 20 |
| 2 | 1 | 7 | 42 | 40 | 20 |
| 3 | 2 | 7 | 43 | 20 | 22 |
| 4 | 3 | 4 | 44 | 21 | 22 |
| 5 | 3 | 9 | 45 | 21 | 23 |
| 6 | 4 | 9 | 46 | 22 | 23 |
| 7 | 5 | 7 | 47 | 23 | 24 |
| 8 | 5 | 8 | 48 | 7 | 24 |
| 9 | 5 | 9 | 49 | 9 | 24 |
| 10 | 6 | 8 | 50 | 7 | 9 |
| 11 | 7 | 8 | 51 | 25 | 26 |
| 12 | 7 | 10 | 52 | 26 | 27 |
| 13* | 7 | 13 | 53 | 27 | 28 |
| 14 | 8 | 9 | 54 | 29 | 30 |
| 15 | 8 | 11 | 55 | 30 | 31 |
| 16* | 8 | 14 | 56 | 31 | 32 |
| 17* | 8 | 15 | 57 | 33 | 34 |
| 18 | 9 | 12 | 58 | 34 | 35 |
| 19* | 9 | 16 | 59 | 35 | 36 |
| 20 | 10 | 11 | 60 | 37 | 38 |
| 21 | 11 | 12 | 61 | 38 | 39 |
| 22 | 13 | 14 | 62 | 39 | 40 |
| 23 | 13 | 25 | 63 | 25 | 29 |
| 24 | 14 | 15 | 64 | 26 | 30 |
| 25 | 14 | 29 | 65 | 27 | 31 |
| 26 | 15 | 16 | 66 | 28 | 32 |
| 27 | 15 | 33 | 67 | 29 | 33 |
| 28 | 16 | 37 | 68 | 30 | 34 |
| 29** | 28 | 17 | 69 | 31 | 35 |
| 30 | 28 | 17 | 70 | 32 | 36 |
| 31 | 17 | 18 | 71 | 33 | 37 |
| 32 | 17 | 21 | 72 | 34 | 38 |
| 33** | 32 | 18 | 73 | 35 | 39 |
| 34 | 32 | 18 | 74 | 36 | 40 |
| 35 | 18 | 19 | | | |
| 36 | 18 | 21 | | | |
| 37** | 36 | 19 | | | |
| 38 | 36 | 19 | | | |
| 39 | 19 | 20 | | | |
| 40 | 19 | 22 | | | |

* Lower inlet doors
** Intermediate deck doors

Table 2. Results of Sensitivity Calculations

| Case | Steam Injec. Rate (kg/s) | Flow Loss Coefficient in Ice Bed Horiz. | Vert. | Heat Transfer Coefficient in Ice Bed | Junction Volumetric Flow Rate ($m^3/s$) 23* (22.9) | 27* (22.9) | 38* (0.46) | 53* (41.75) |
|---|---|---|---|---|---|---|---|---|
| 1 | 20. | 3. | .2 | Default | -17. | 22. | .8 | -8.4 |
| 2 | 20. | 100. | .2 | Default | -8. | 14. | .8 | -2 |
| 3 | 200. | 3. | .2 | Default | -28. | 83. | 2. | -18. |
| 4 | 200. | 100. | .2 | Default | -12. | 65. | 2. | -3. |
| 5 | 1000. | 3. | .2 | Default | 70. | 170. | 3. | -5. |
| 6 | 1000. | 100. | .2 | Default | 70. | 170. | 3. | 10. |
| 7 | 200. | 0. | 10. | Default | -23. | 75. | 2.5 | -22. |
| 8 | 200. | 0. | 100. | Default | -19. | 70. | 2.5 | -3. |
| 9 | 200. | 3. | .2 | 10.*Default | -10. | 60. | 2.5 | -5. |
| 10 | 200. | 3. | .2 | .1*Default | -24. | 64. | 3.5 | -10. |

* These numbers refer to the junction numbers listed in Table 1. The flow areas ($m^2$) are listed in parentheses below the junction numbers.

Table 3. Ice Melting in Sensitivity Calculations

| Case | Time (s) | Mass of Ice Melted (kg) Column 1 | Column 2 | Column 3 | Column 4 |
|---|---|---|---|---|---|
| 1 | 199.5 | 140 | 1050 | 1110 | 160 |
| 2 | 200.5 | 80 | 910 | 1050 | 80 |
| 3 | 49.4 | 780 | 6760 | 6730 | 790 |
| 4 | 50.4 | 410 | 6200 | 6170 | 410 |
| 5 | 50.1 | 11100 | 45840 | 45620 | 11040 |
| 6 | 45.6 | 8960 | 43580 | 43660 | 8820 |
| 7 | 50.4 | 440 | 7390 | 6620 | 430 |
| 8 | 50.5 | 1500 | 5540 | 5530 | 1500 |
| 9 | 50.1 | 130 | 6240 | 7240 | 130 |
| 10 | 50.5 | 1040 | 2110 | 2110 | 1040 |

* The initial mass of ice in each column is 2.755E5 kg.

Table 4. Mole Fractions before First Upper Plenum Burn in S1HF Fans-off

| Compartment Number | Mole Fraction Steam | Oxygen | Hydrogen |
|---|---|---|---|
| 1 | 0.630 | 0.011 | 0.317 |
| 2 | 0.615 | 0.011 | 0.331 |
| 3 | 0.615 | 0.011 | 0.331 |
| 4 | 0.630 | 0.011 | 0.317 |
| 5 | 0.422 | 0.013 | 0.516 |
| 6 | 0.393 | 0.084 | 0.042 |
| 7 | 0.599 | 0.011 | 0.346 |
| 8 | 0.392 | 0.011 | 0.554 |
| 9 | 0.599 | 0.011 | 0.346 |
| 10 | 0.709 | 0.016 | 0.212 |
| 11 | 0.654 | 0.038 | 0.163 |
| 12 | 0.701 | 0.019 | 0.211 |
| 13 | 0.010 | 0.160 | 0.225 |
| 14 | 0.013 | 0.160 | 0.225 |
| 15 | 0.084 | 0.129 | 0.300 |
| 16 | 0.015 | 0.154 | 0.251 |
| 17 | 0.024 | 0.196 | 0.038 |
| 18 | 0.025 | 0.192 | 0.060 |
| 19* | 0.028 | 0.189 | 0.070 |
| 20 | 0.026 | 0.192 | 0.057 |
| 21 | 0.027 | 0.202 | 0.006 |
| 22 | 0.027 | 0.201 | 0.012 |
| 23 | 0.027 | 0.203 | 0.003 |
| 24 | 0.027 | 0.203 | 0.000 |
| 25 | 0.009 | 0.158 | 0.236 |
| 26 | 0.014 | 0.156 | 0.243 |
| 27 | 0.016 | 0.154 | 0.248 |
| 28 | 0.013 | 0.154 | 0.250 |
| 29 | 0.023 | 0.157 | 0.227 |
| 30 | 0.056 | 0.145 | 0.253 |
| 31 | 0.048 | 0.145 | 0.259 |
| 32 | 0.036 | 0.147 | 0.259 |
| 33 | 0.068 | 0.136 | 0.282 |
| 34 | 0.070 | 0.136 | 0.281 |
| 35 | 0.070 | 0.136 | 0.277 |
| 36 | 0.064 | 0.139 | 0.270 |
| 37 | 0.022 | 0.151 | 0.257 |
| 38 | 0.034 | 0.147 | 0.261 |
| 39 | 0.037 | 0.147 | 0.262 |
| 40 | 0.034 | 0.148 | 0.260 |

*Combustion begins in compartment 19.

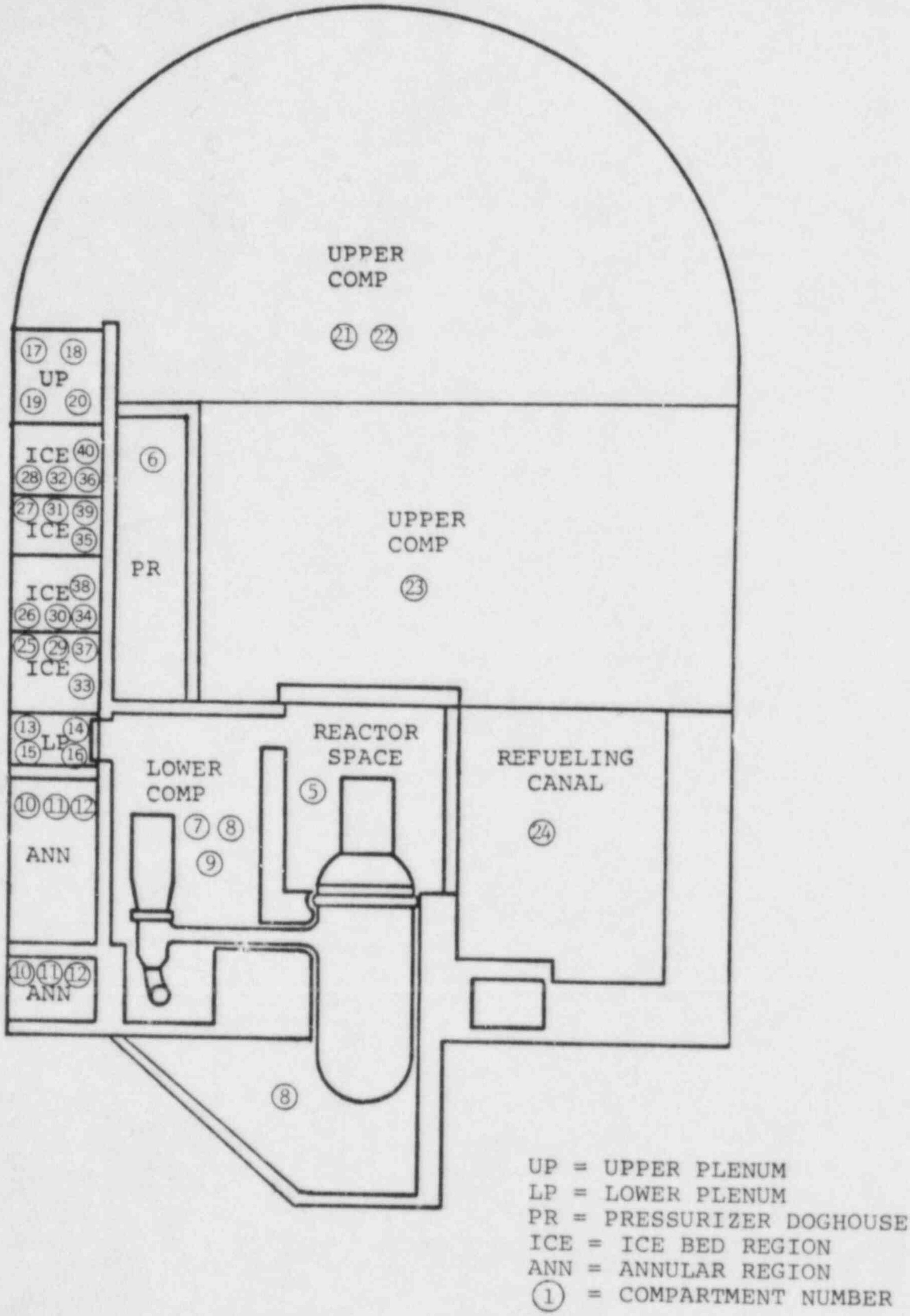
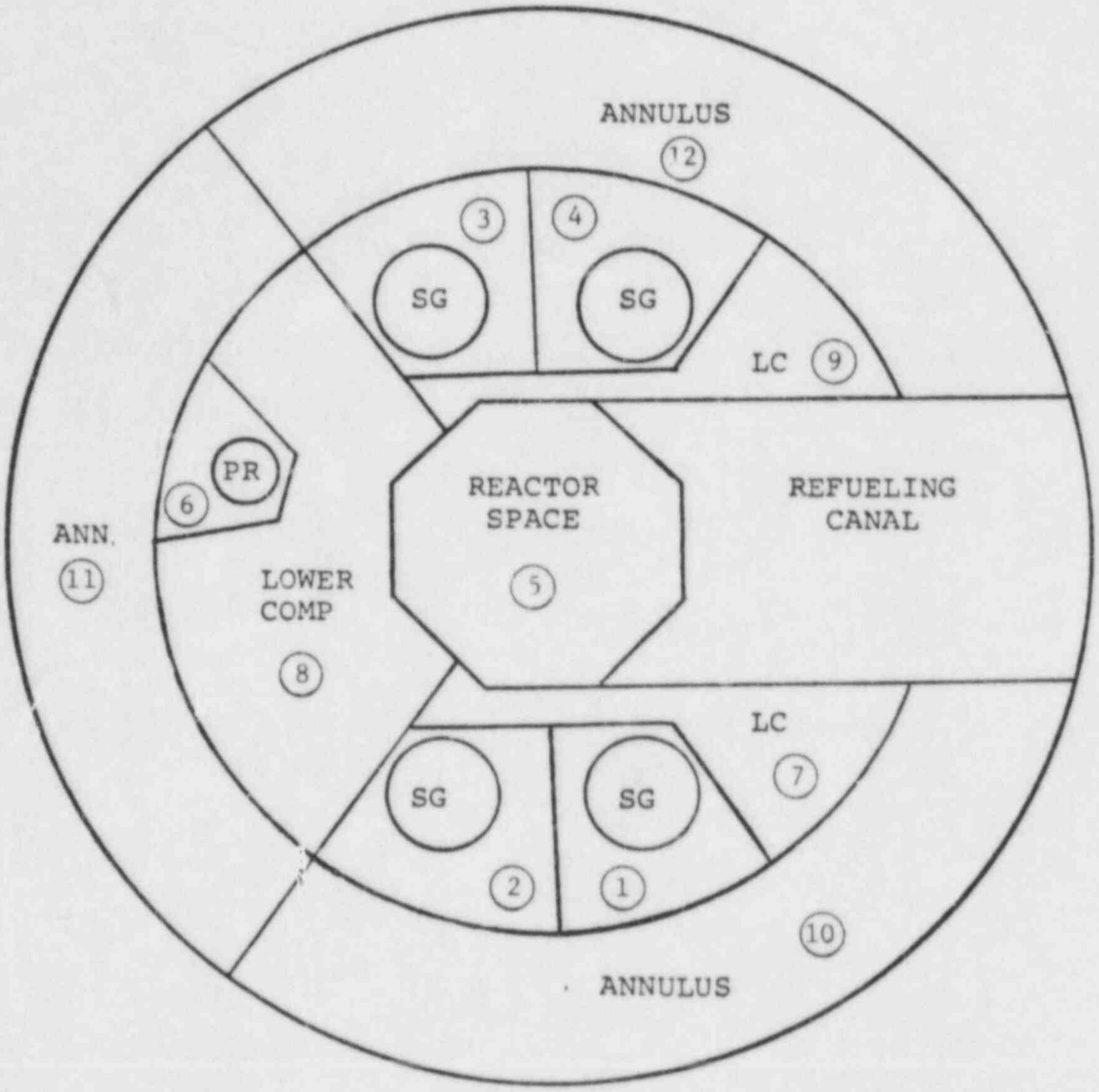
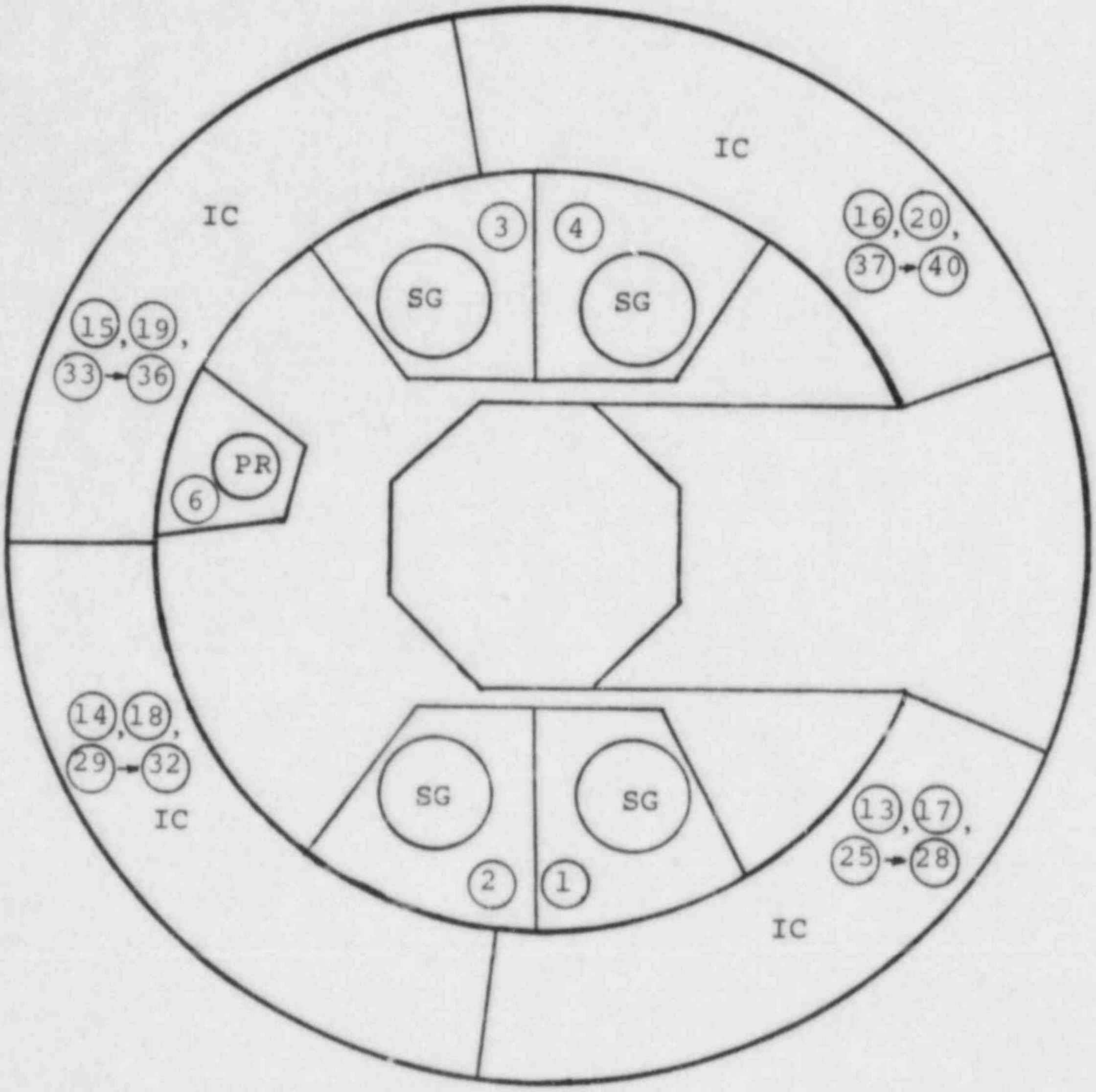



Figure 1. Containment Nodalization




SG = STEAM GENERATOR DOGHOUSE
PR = PRESSURIZER DOGHOUSE
1 = COMPARTMENT NUMBER

Figure 2. Nodalization of Lower Compartment




IC = ICE CONDENSER
SG = STEAM GENERATOR DOGHOUSE
PR = PRESSURIZER DOGHOUSE
1 = COMPARTMENT NUMBER

Figure 3. Top View of Ice Condenser Nodalization

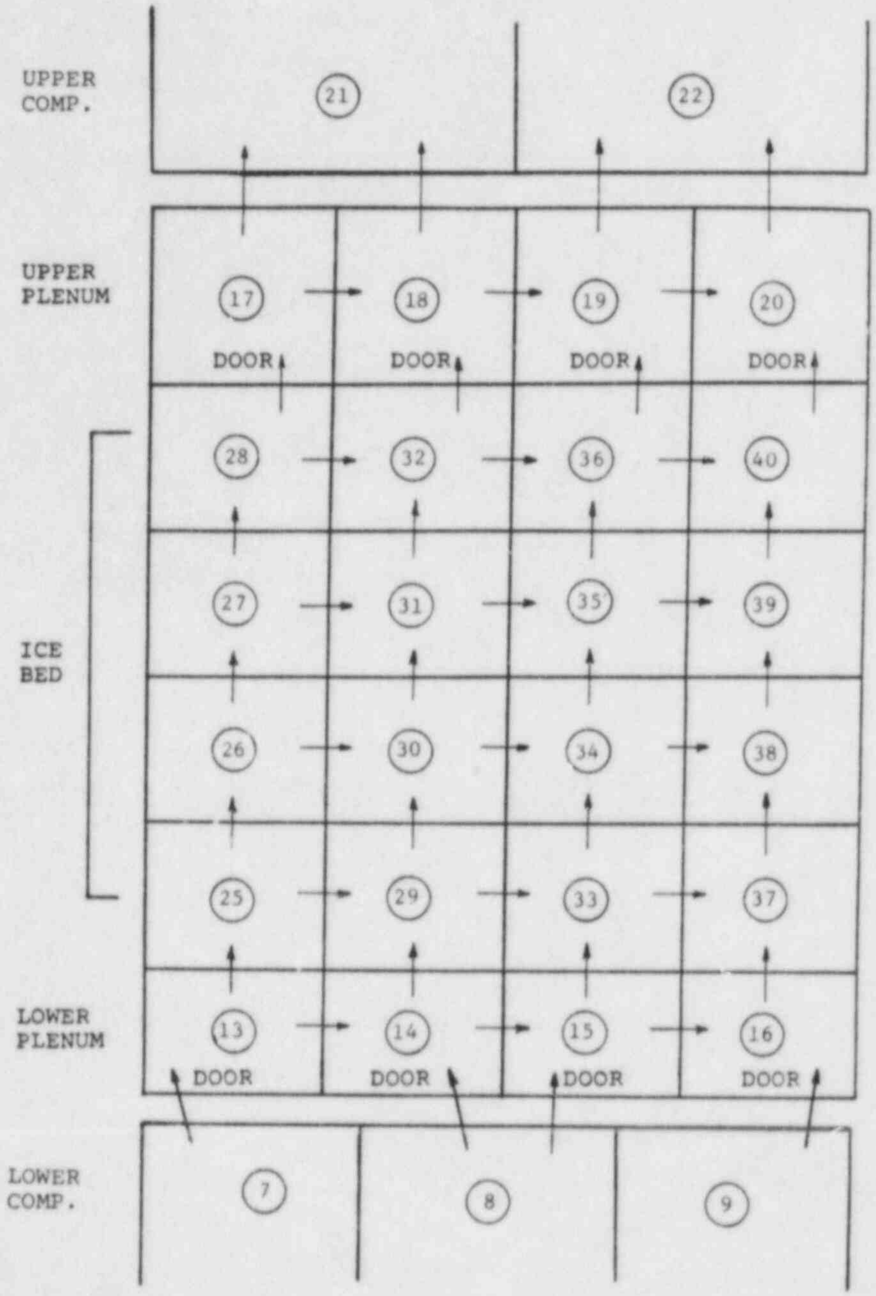



Figure 4. "Unwrapped" View of Ice Condenser Nodalization

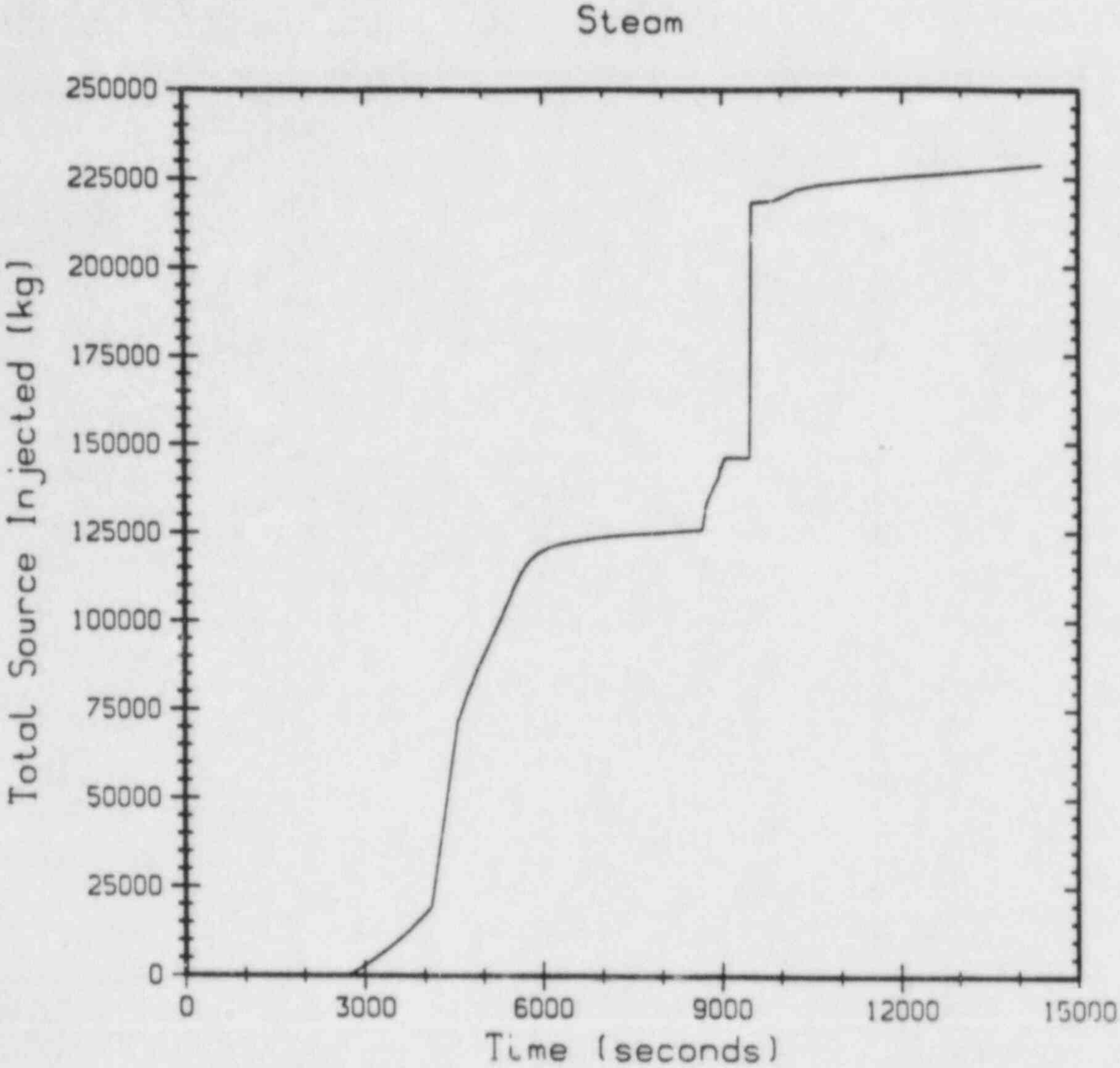



Figure 5. Steam Source for TML3' Calculation

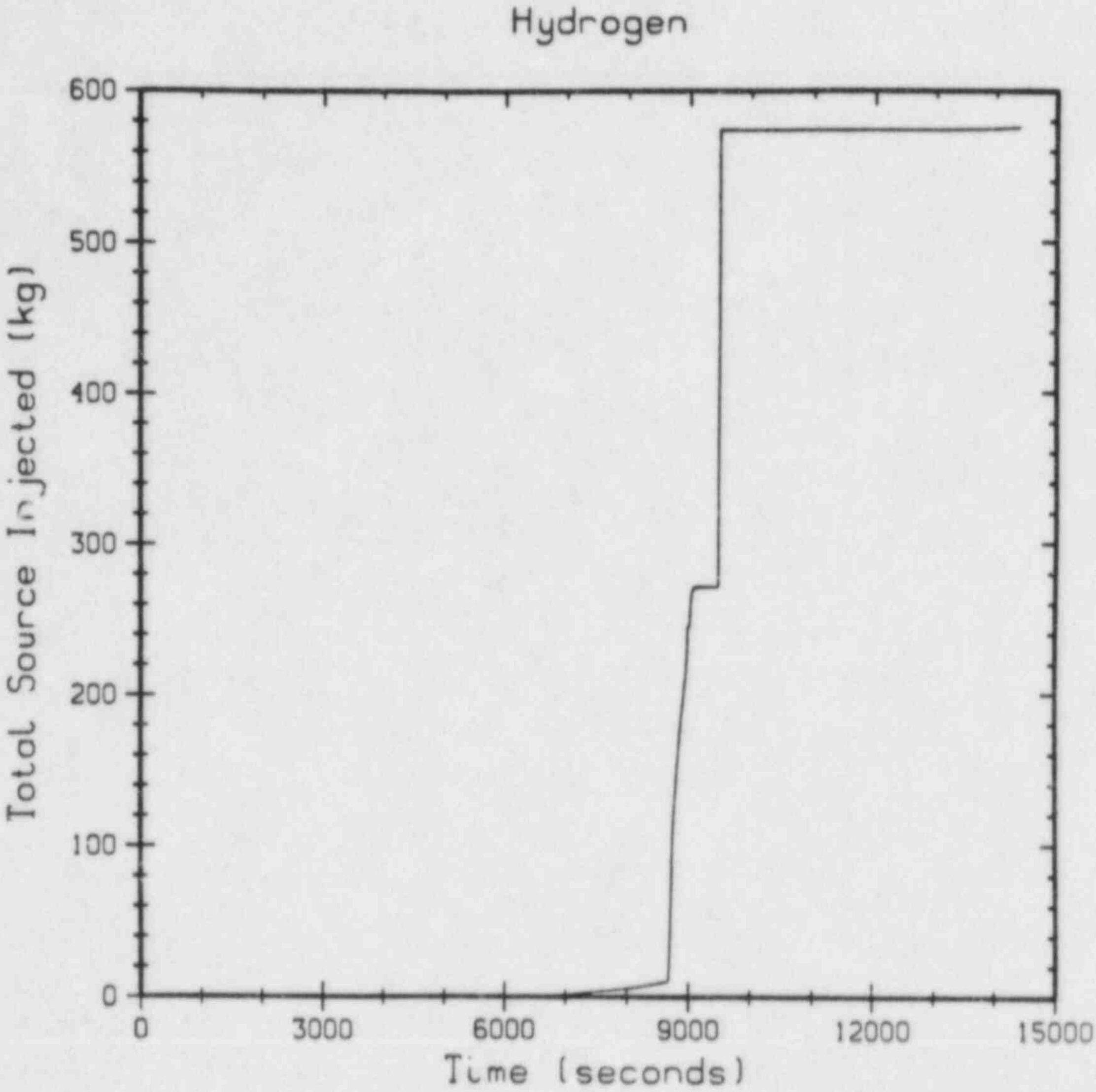
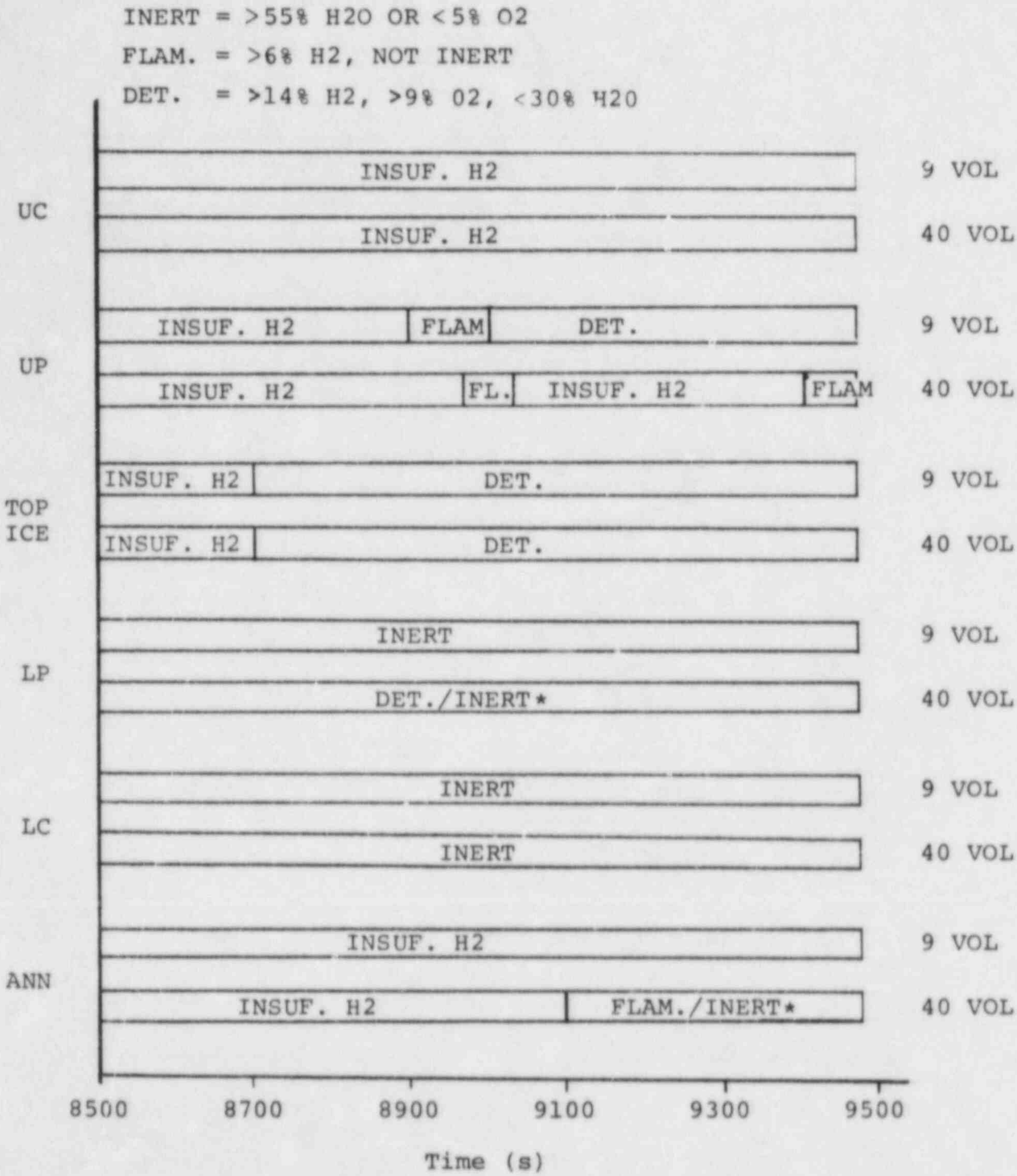



Figure 6. Hydrogen Source for TMLB' Calculation




* Varies from compartment to compartment in this region.

Figure 7. Flammability of Containment after Core Slump

INERT = >55% $H_2O$ OR <5% $O_2$

FLAM. = >6% $H_2$, NOT INERT

DET. = >14% $H_2$, >9% $O_2$, <30% $H_2O$

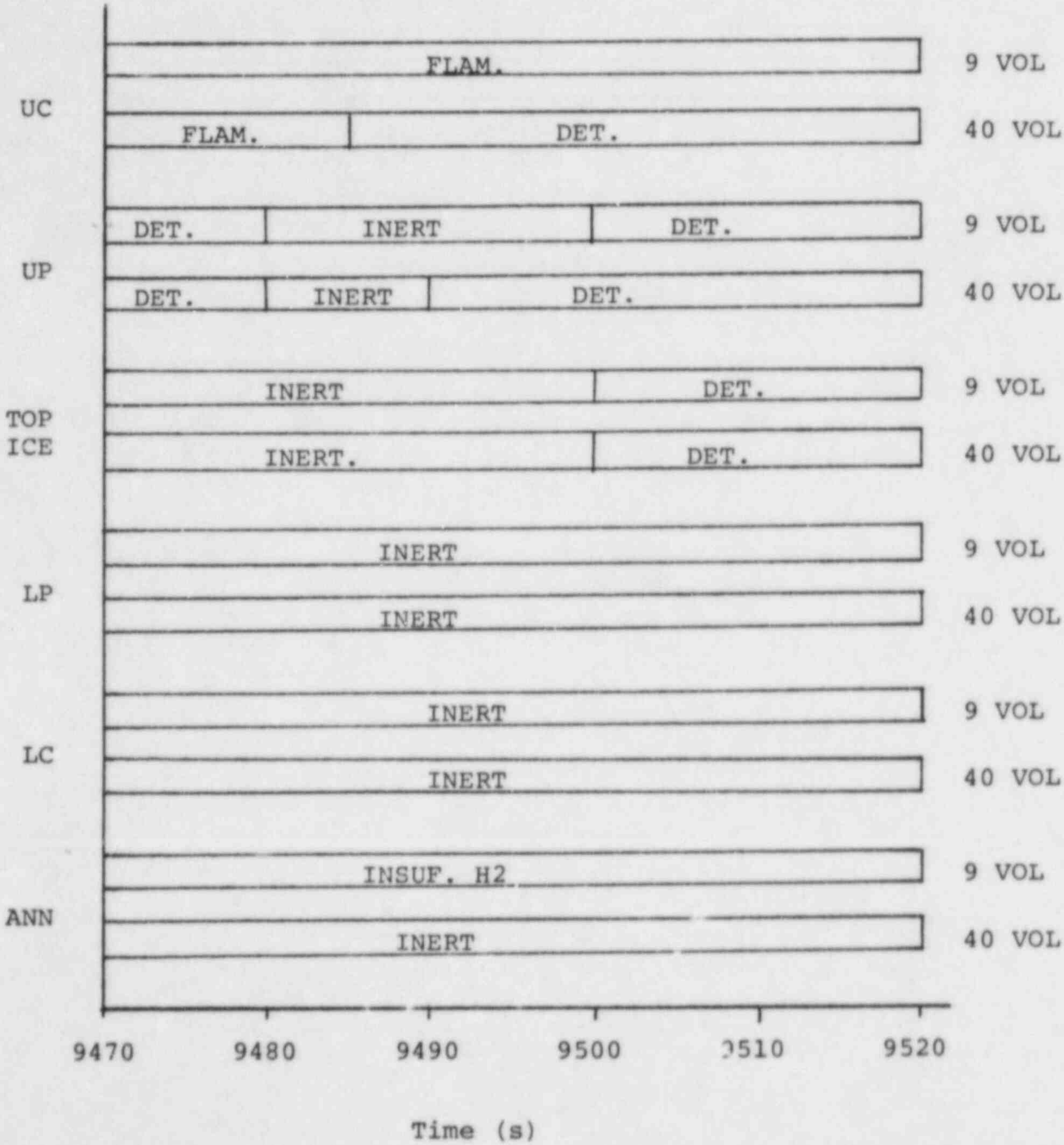



Figure 8. Flammability of Containment at Vessel Breach

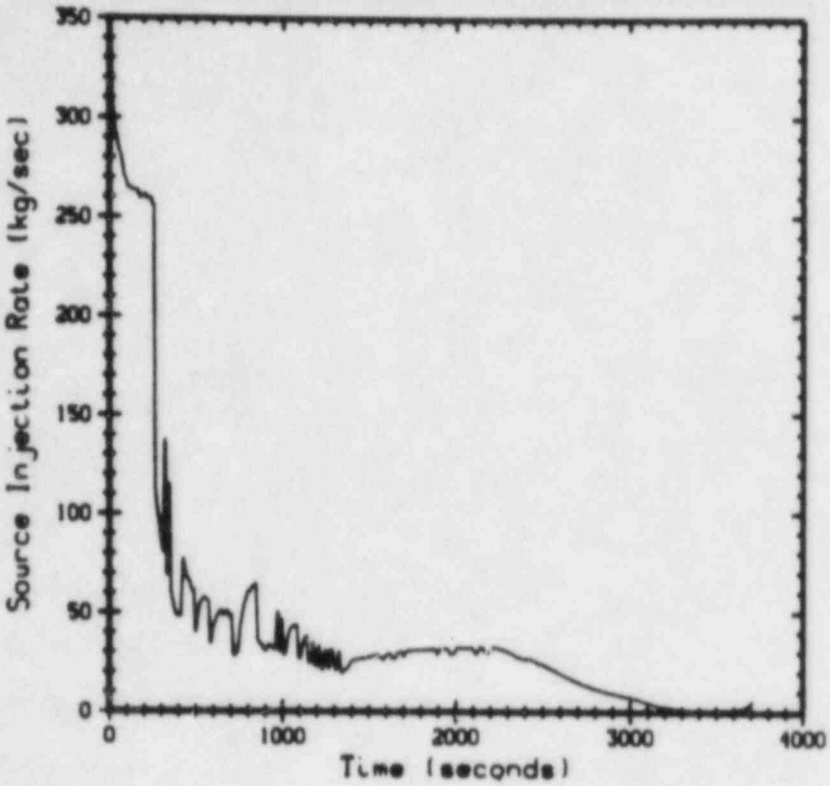



Figure 9. Steam Source for $S_1HF$ Calculations

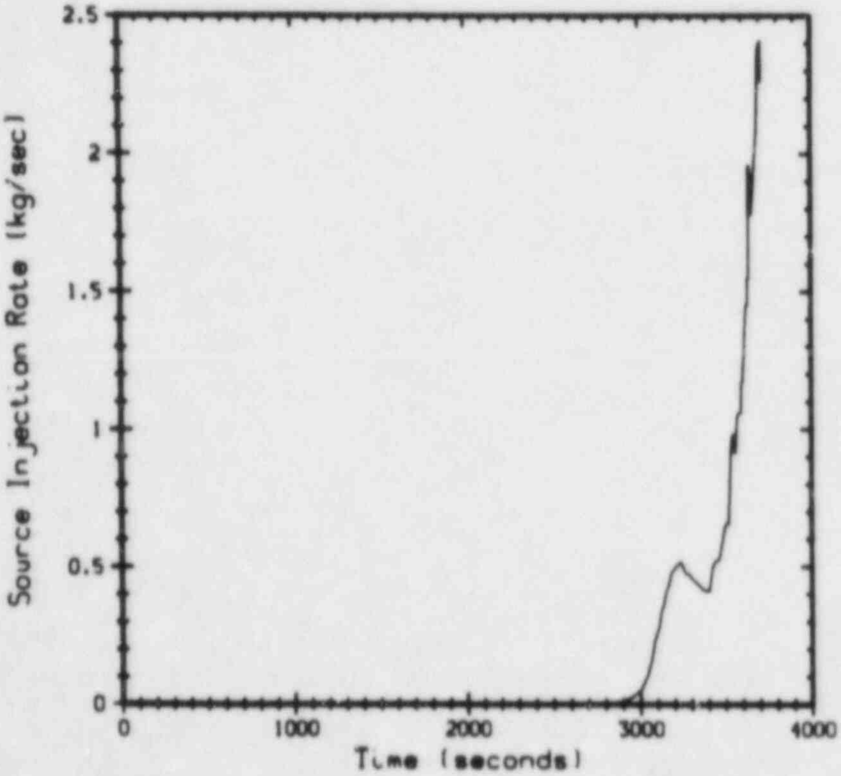



Figure 10. Hydrogen Source for $S_1HF$ Calculations

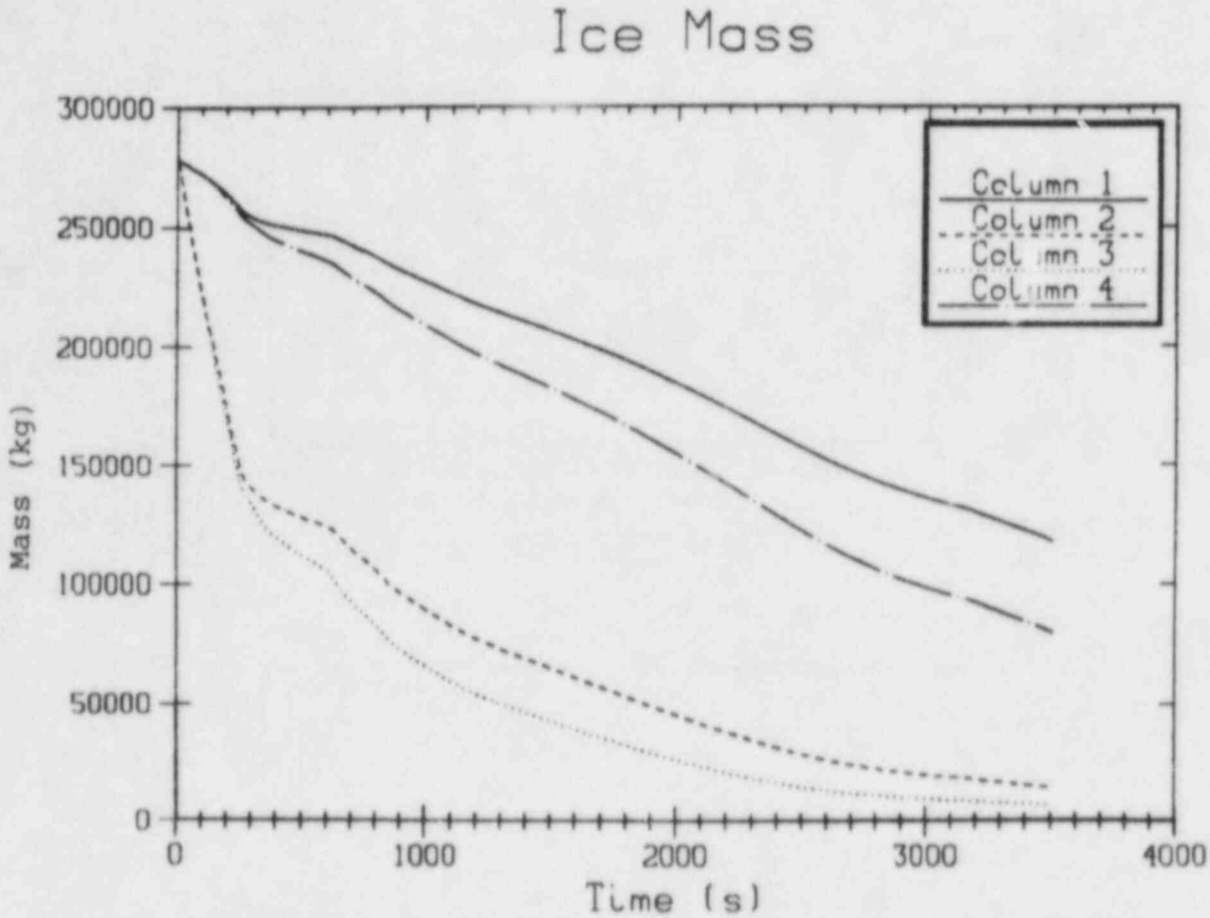



Figure 11. Ice Mass for $S_1$HF Fans-on Calculation

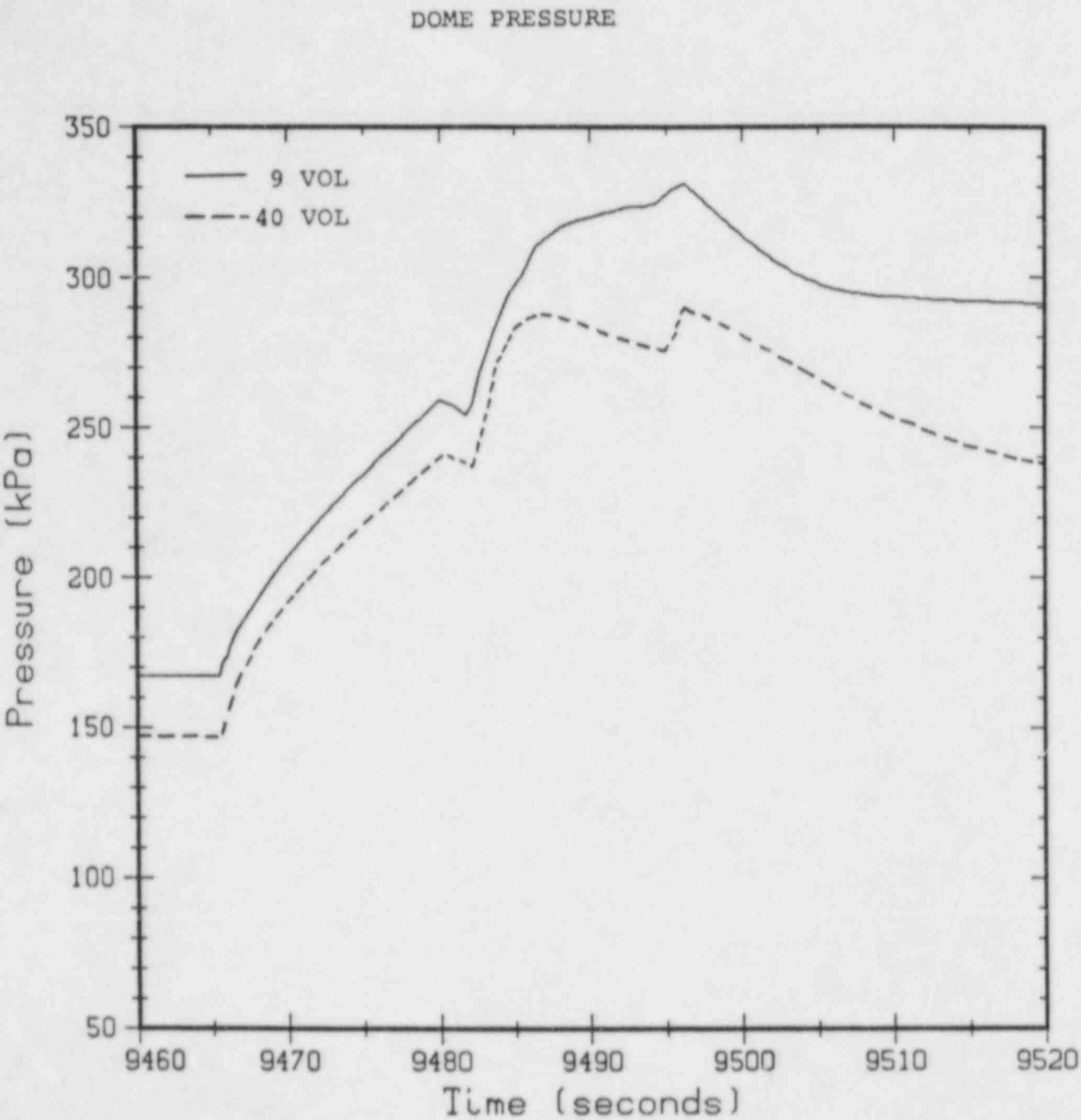



Figure 12. Containment Pressures Near Vessel Breach in TMLB'

SAND85-2434A

# Analysis of Dependent Failures and External Events

by

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## Abstract

This paper presents the results of research performed in the area of common cause failures and external event analyses for probabilistic risk assessment. This work is being performed in support of the Risk Methods Integration and Evaluation Program (RMIEP) which is the first full scope probabilistic risk assessment (PRA) being performed under the auspices of the US Nuclear Regulatory Commission since the Reactor Safety Study. The two supporting programs are the Dependent Failure Methodology Program and the PRA Methods Development Program, both of which are sponsored by the Division of Risk Analysis and Operations, US Nuclear Regulatory Commission. These programs have the goal of developing new risk assessment methods and integrating both new and existing methods into a uniform procedure for performing an in-depth PRA with consistent levels of analysis for external, internal and dependent failure scenarios.

In this paper, we will illustrate the underlying common features of dependent failure and external event analysis and demonstrate that there are unified approaches which can be used in treating both with a common level of detail. Emphasis in these approaches is on screening procedures which permit a step-by-step evaluation of the relative importance of the various common cause and external event scenarios being considered.

## I. COMMONALITIES BETWEEN COMMON CAUSE FAILURES AND EXTERNAL EVENTS

Table 1 presents examples of the types of analyses to be discussed. Under the subheading "Common Cause Failures" are such agents as high temperature/moisture, vibration, corrosion, etc. Under the "External events" are such energetic events as earthquakes, fires, tornadoes, etc. The common cause failures listed fall under the general category of abnormal ambient environments. There are, of course, other general classes of common cause failures such as those induced by human error, test and maintenance activities and the broad, but generally undefined area of common cause failures due to design defects. We focus here on common cause failures due to abnormal ambient environments, however, because in these cases (as well as in the cases of external events) the common mode failures are induced by a clearly identifiable root cause agent.

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Table 1

EXAMPLES OF TYPES OF ANALYSIS TO BE DISCUSSED

| COMMON CAUSE FAILURES (CMF) | EXTERNAL EVENTS |
|---|---|
| HIGH TEMPERATURE/MOISTURE | EARTHQUAKE |
| VIBRATION | FIRE |
| CORROSION/CRUD | INTERNAL FLOODING |
| RADIATION | TORNADO |
| ETC. | ETC. |

The root cause agents to be discussed are a subset of the more general set being considered by the NRC-sponsored Root Cause Identification Program, which is being performed at the Idaho National Engineering Laboratory. Table 2 presents a list of the types of root causes that are being examined in this program. Data sets being identified include the licensee event report (LER) data base, the IPRDS (Ref. 1) and the NPRDS (Ref. 2) data bases. It is planned that data for the quantification of the RMIEP accident sequences will be obtained from this NRC program as well as ongoing programs sponsored by the Electric Power Institute.

Table 2

- DESIGN INADEQUACY
- FAULTY MANUFACTURER
- IMPROPER INSTALLATION
- IMPROPER TEST
- IMPROPER CALIBRATION
- IMPROPER MAINTENANCE
- IMPROPER CONFIGURATION CONTROL
- IMPROPER OPERATION
- AGING (WEAR-OUT)
- HARSH ENVIRONMENTS
- OTHER CAUSES

In this section, we define and discuss a quantitative means of evaluating dependencies between failures and illustrate various physical situations in which dependencies play a significant role. The underlying feature of dependent failure events is the existence of correlation between failures of elements of a cut set. The typical situation is given by equation 1.

$$\text{Accident Sequence} = IE \left| \; .... + C_1\, C_2\, C_3 + ..... \right|$$

where $C_i$ are component failures (1)

IE is the initiating event

If all three components fail and the initiating event occurs, then the accident sequence occurs with its attendant core damage situation. If the component failures are independent, then the probability of the cut set is just equal to the product of the probabilities of the individual component failures. However, in the cases of dependent failures and external events, the component failures are often linked via a) location, b) common or correlated responses, c) correlated failure thresholds (fragility). In addition, the common cause agent may or may not also cause the initiating event to occur.

Failure of individual components may be described in terms of responses and fragilities. A <u>response</u> is a random variable describing the excitation or environment that the component experiences (e.g., temperature, vibration level, etc.). Similarly, a <u>fragility</u> is a random variable describing the failure threshold of the component (in the same units as the response). Both of these quantities are random variables, because, in general, we do not have an exact description of the response or the failure threshold for component. And in many cases, such as earthquakes, the actual input to the system (groundshaking) is a random variable itself. So both the response and the fragilities must be described in terms of probability distribution functions. The failure of a single component can be computed by the so-called interference function (Ref. 3) described in Equation 2.

$$P_F = \int_0^{\infty} F_{FRAG}\,(r)\; f_{RESP}\,(r)\; dr \qquad (2)$$

In this equation, F is the cumulative distribution function for the fragility random variable, while f is the probability density function for the response random variable. Thus, given the two distributions for the random fragility and response variables, one can analytically or numerically evaluate this integral and hence compute the component probability of failure (conditional on the presence of the agent causing the response).

A commonly used distribution for responses and fragilities is the log-normal distribution. This is particularly useful because it is defined on the range 0 to infinity which is appropriate since most responses are non-negative quantities. A log-normal distribution is defined in terms of two parameters. The first is the median of the distribution and the second is the uncertainty variable ß which is the standard deviation of the logarithms of the random variable. It may be shown that the quantity ß is approximately equal to the coefficient of variation for the distribution. If log-normal distributions are used to describe the response and fragility variables, then the probability of failure of the component is given by Equation 3

$$PF = 1 - \Phi \left[ \frac{- \ln \frac{M_r}{M_f}}{\sqrt{\beta_R^2 + \beta_F^2}} \right] \tag{3}$$

where

- $M_R$ = median of the response
- $M_F$ = median of the fragility
- $\beta_R$ = uncertainty of the response
- $\beta_F$ = uncertainty of the fragility
- $\Phi$ = normal distribution function with mean zero and standard deviation equal to unity

Thus knowing the medians of the response and fragility and the associated uncertainties one can directly calculate the probability of failure of a component using readily available published tables of the normal distribution.

When calculating the probability of a cut set, however, we are dealing with the simultaneous failure of two or more components. Here we must consider the possibility of correlation (dependence) between these individual component failures. Correlation implies a pairwise relationship between two random variables. As an example of this, consider the simple structure being shaken by ground motion due to an earthquake shown in Figure 1.

Components A and component B are located on adjacent floors near the top of the building. On the left hand side of Figure 1, there is a plot of acceleration of component A versus the acceleration of component B. Each point corresponds to a different earthquake. Small earthquakes give rise to small accelerations in components A and B, whereas large earthquakes give rise to large accelerations. However, as illustrated, the accelerations of components A and B both tend to be either high together or low together, depending on the type of earthquake impinging on the structure. Thus, the acceleration variables for components A and B are said to be correlated. Note that correlation does not imply that the variables be either large or small, only that they vary in a similar fashion.

The correlation between two component failures depends on both the correlation between responses and the correlation between the fragilities, as well as the uncertainties in the responses and the fragilities. This is illustrated in Equation 4, which explicitly gives the correlation coefficient $\rho$ between two component failures in terms of correlations between response and fragility, for the case where the variables are described by log-normal distributions.

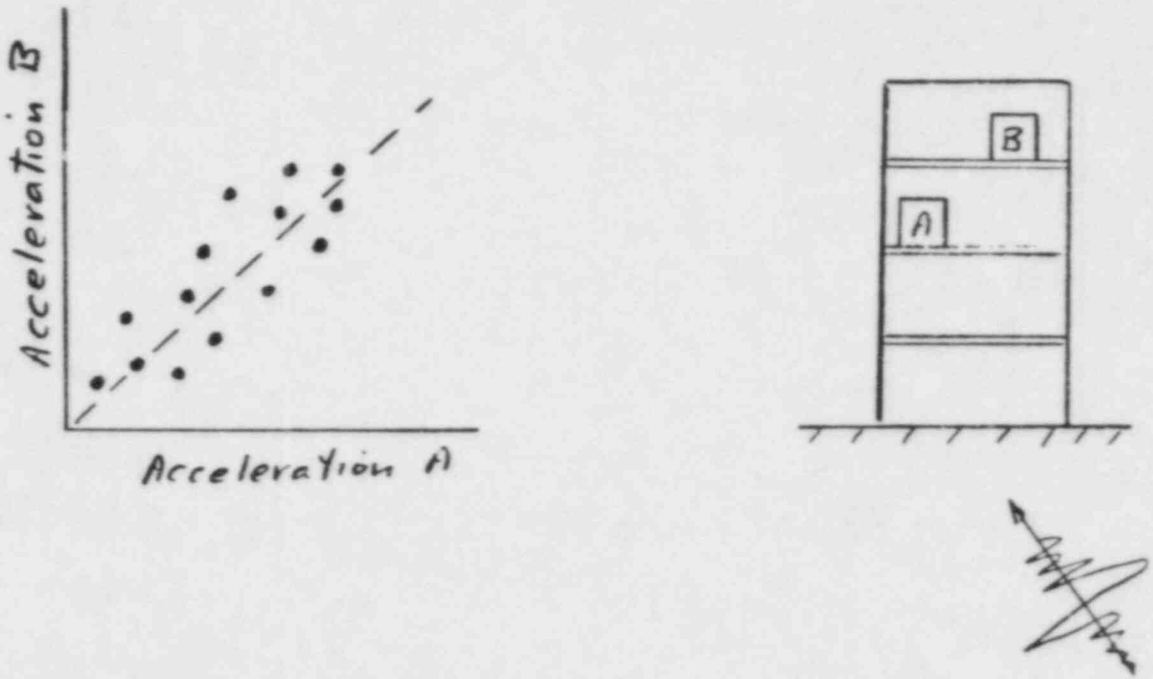


Figure 1. Example of Highly Correlated Responses

$$\rho = \frac{\rho_{RARB}\ \beta_{RA}\ \beta_{RB} + \beta_{FAFB}\ \beta_{FA}\ \beta_{FB}}{\sqrt{\beta_{RA}^{2} + \beta_{FA}^{2}}\ \sqrt{\beta_{RB}^{2} + \beta_{FB}^{2}}} \tag{4}$$

It is important to note that the magnitude of the correlation between two component failures depends significantly on the uncertainties in the response and fragility variables.

Table 3 presents typical examples of responses, fragilities and correlations in common situations arising in dependent failure and external event analysis. In the case of fire, for example, the response variable might be either temperature or heat flux. Given a fire occurring in a room, we can use computer codes to compute the temperatures at various points in the room. Further, given uncertainties in the input variables that the computer code uses to calculate the temperatures, we can compute uncertainties in the computed temperatures at the various points in the room. Similarly, from these computations, we can compute the correlation between the temperatures. The fragilities of the components with respect to temperature, however, must be determined by test. Also, the uncertainty in the fragilities must come from these tests. In principle, one could also determine the fragility correlations by making pairwise tests of components exposed to temperature. In practice, no such tests for fragility correlation have yet been performed for any of the harsh environment agents or external events. Similarly, for the external events of earthquake and flood, one can identify the responses, compute uncertainties and correlations in the response variable and then make use of tests to characterize the fragility and its correlation.

For the harsh environment case of high temperature/moisture (such as would be caused by a steam pipe break or steam leakage from a valve flange) one can identify the local relative humidity as a response variable. In this case, it is more difficult to identify the uncertainty in the response unless one is dealing with a large steam pipe break which would effectively flood the room with steam. Estimates of the correlation response must come from observation and judgement of the physical situation. Thus, for example, if one is dealing with two valve motor operators located in close conjunction with a leaking valve flange, one could reasonably assume that both operators are seeing the same steam environment and thus the responses would be highly correlated. To determine the fragility of equipment with respect to a high temperature, high moisture environment, one has a certain amount of qualification test data plus the results of operating experience in nuclear power plants as contained in the LERs. Again, the fragility uncertainty correlation must be estimated based on judgement and a knowledge of the failure modes of the equipment due to the presence of high temperature and steam.

Another common example of harsh environment is that of local vibration, which might be caused by the nearby presence of large rotating machinery (such as RCIC pumps in a BWR). In this case, the response variable is the local acceleration seen by the nearby components. The uncertainty in the response would come from observation at the site. Here, one can actually measure the acceleration levels induced by the presence of the large rotating machinery and, further, one can characterize the uncertainty in the response knowing how

Table 3

EXAMPLES OF RESPONSES, FRAGILITIES AND CORRELATIONS

| | FIRE | EARTHQUAKE | FLOOD | HIGH TEMP/ MOISTURE | LOCAL VIBRATION |
|---|---|---|---|---|---|
| RESPONSE | TEMP FLUX | SPECTRAL ACCELERATION | WATER HEIGHT | LOCAL RELATIVE HUMIDITY | ACCELERATION |
| RESPONSE UNCERTAINTY | COMPUTE | COMPUTE | COMPUTE | ? | OBSERVATION |
| RESPONSE CORRELATION | COMPUTE | COMPUTE | 1 | JUDGEMENT, OBSERVATION | 1 |
| FRAGILITY | TEST | TEST | TEST | LERS AND TEST | TEST |
| FRAGILITY UNCERTAINTY | TEST | TEST | 0 | ? | TEST |
| FRAGILITY CORRELATION | TEST* | TEST* | 1 | ? | TEST* |

the level of acceleration changes with different modes of power operation. In this case, the components near the large rotating machinery would have a response correlation of nearly unity because they would all be seeing the same environment. The fragility must be estimated from tests, and indeed, such test data are available.

Proper inclusion of correlation when evaluating cut sets is crucial because the presence of correlation can change the probability of a cut set by orders of magnitude. Figure 2 schematically illustrates this situation. This figure shows the probability of the simultaneous failure of two components A and B as a function of the correlation between the failures (such as defined by Equation 4). If the component failures are independent, then $\rho$ equals zero and the probability of both A and B failing is just the product of their probabilities, i.e., $P_A P_B$. If however the component failures are fully correlated, and $\rho$ equals to one, then the joint failure probability is given by the minimum of the two individual failure probabilities.

Given that conditional failure probabilities of risk significant components are typically on the order of 0.1 to 0.01 in magnitude, then clearly the difference between these two limiting cases of $\rho$ equal to zero and $\rho$ equal to one is on the order of two to four orders of magnitude. For intermediate values of $\rho$, the probability of the joint failure of components A and B can be computed using published tables, or for the case of more than two components, existing computer codes developed by the Nuclear Regulatory Commission in its Seismic Safety Margins Research Program can be used. In fact, the computer code, SEISIM (Ref. 5), was developed explicitly for evaluating the probabilities of large order cut sets given arbitrary degrees of correlation between the component failures. SEISIM can consider cut sets including up to 15 variables, 10 of which may have arbitrary correlation and five of which are assumed to be random independent variables. SEISIM can handle failures and components which are described either by normal distributions or log-normal distributions or combinations thereof. Correlations between responses and fragilities for each pair of components in the cut set must be numerically specified.

Figure 2 illustrates an important aspect of correlation; namely, that for large values of $\rho$ (near one) or for small values of $\rho$ (near zero) the joint failure probability curve is nearly flat. This implies that if we are dealing with component correlations on the order of 0.3 or less, one can generally neglect the correlation and assume that the component failures are independent. Conversely, if we are dealing with correlation coefficients of 0.7 or greater, one can assume that the failure probabilities of the two components are fully correlated. This observation can greatly simplify the work of the analyst.

In general, it can be seen that the response correlation can be either computed or measured for most external events or harsh environments agents of interest. The fragility correlation, however, is generally unknown. But in principal, it can be found by pairwise testing of components. In many cases, where the failure mode is clearly identifiable, such as the cases of electrical equipment failure due to fire where failure occurs by melting of solder joints or temperature dependent failure of semi-conductors, there will be a very high level of fragility correlation due to the relatively

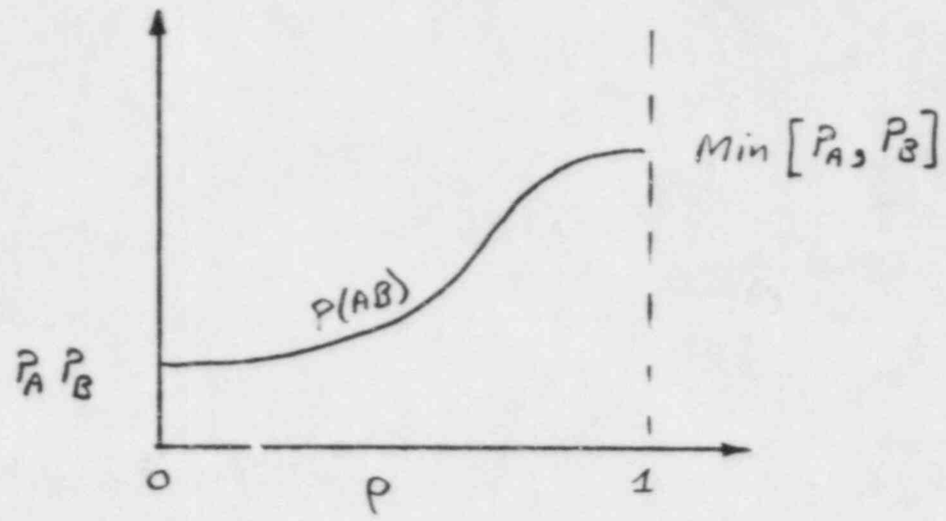


Figure 2. Effect of Correlation on Joint Failure Probability

unambiguous failure mode. Thus, in estimating fragility correlations, an understanding of the equipment and its susceptibility to the agent can aid the analyst in evaluating an appropriate fragility correlation. Finally, it should be noted that if we are dealing with similar components for which the uncertainties in the responses and the fragilities are about the same (which is often the case) then the correlation between the two component failures is given by Equation 5 below.

$$\rho = 0.5\ \rho_{RARB} + 0.5\ \rho_{FAFB} \tag{5}$$

This equation shows that the total correlation is split equally between the correlation between the responses and between the fragilities. And thus in evaluating the cut set, the analyst must put equal effort into defining the response correlations and the fragility correlations in order to get an accurate result.

It has been illustrated there exists a common framework for evaluating and quantifying dependent failures whether due to harsh environment or external events. The exact approach which can be used for quantifying such cut sets depends on the nature of the data available. In general, there are three different types of data.

a. Response and Fragility Data

In this case, one can make direct use the correlation coefficient approach, combining this with an estimate of the common cause occurrence frequency in order to evaluate the accident sequences. This approach is being used in RMIEP for the fire, flood, earthquake and major steam pipe break analyses.

b. LER Failure Rate Data

In this case, the only data available are those obtained from the component failure data bases such as the LERs, the IPRDS and the NPRDS. The available data consists of the number of component failures which have occurred due to an identifiable common cause agent over a number of years of reactor operation. Further, it is generally possible to identify the situations in which two or more components have failed due to the same root cause agent. From this data, one can estimate the fraction of failures which have occurred in a common cause mode relative to the total number of failures observed. This ratio is generally referred to as a β factor. A variety of different definitions and approaches have been used over the years to quantify such a fraction. In general, the β factor is related to a correlation coefficient, but does not identify the relative correlations between response and fragility.

To make use of such a generalized β factor, one identifies the components which are susceptible to each common cause agent. On the safety system fault trees, one modifies the basic component failure events to include both the random failure of the component as well as the common mode failure due to the specific root cause. This is illustrated in Figure 3 where the basic component failures for components A and components B are illustrated showing the link through the common cause failure event (CMF). When the fault trees are reduced using the laws of Boolean logic, one will find two events in the accident sequence involving the joint failure of A and B; namely, the product of the random failures $P_A P_A$, and in addition, a single event CMF which corresponds to the common mode failure. This is then quantified by use of the generalized β factor.

As mentioned above, a number of different generalized β factor definitions are currently in use, starting with the original basic definition by Fleming (Reference 6). Current definitions include the binomial failure rate model, the modified β factor, the C factor, and the Multiple Greek Letter method. A number of papers (e.g., Ref. 7) have reviewed and evaluated the advantages of the different definitions. In general, though, it has been found that if a consistent level of definition of the common cause agent and its consequences are used, the actual numerical approach used in evaluating the cut sets is of secondary importance. The use of generalized β factors will be used in the RMIEP analysis for consideration of corrosion, crud, strainer blockage, design defects and the effect of improper procedures.

c. Enhanced Failure Rate Data

Here, the data consists of failure rates of single components which have been operating in a harsh environment. These include, for example, the stress factors which are reported in Reference 8. Available data include the effect of vibration on electrical equipment. These data may be used directly in quantification of the cut sets. In the RMIEP analysis, this approach will be used to include the effects of local vibration on susceptible electrical equipment.

A review of the data bases for component failures and for harsh environment occurrence frequencies was made (Ref. 9). Not surprisingly, the review showed that data bases are very well developed in some areas and almost non-existent in others. In the case of seismic analyses, data and methods for calculating earthquake hazard frequencies have already been developed. In addition, a comprehensive generic component failure data base exists which relates component failure to the spectral acceleration at the base of the equipment. When used in conjunction with site specific fragility analyses of components identified during a plant walkdown, one finds that the fragility data base is reasonably complete for nuclear power plant components.

In the case of fire, a limited number of data exist on fire occurrence frequencies. In general one has access to the nuclear insurance industry which keeps a complete record of fire occurrences in nuclear power plants from which to estimate these frequencies. Distributions on the size of fires which could occur must come from a site walkdown and an estimate of the combustibles available in any given area to feed a fire. Very little data exist on

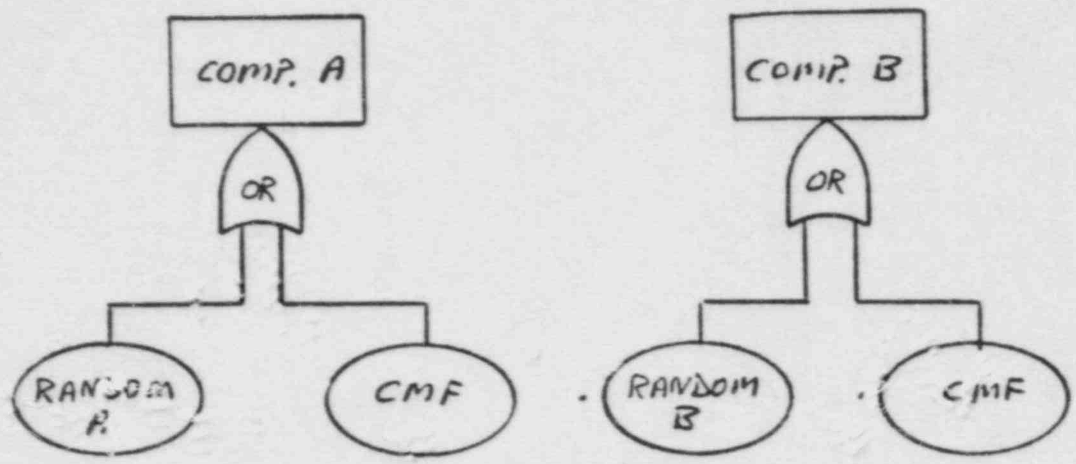


Figure 3. Inclusion of Common Mode Failure Event on Fault Trees

component fire damage thresholds. To augment this data, a current NRC-sponsored program being performed at Sandia National Laboratories is performing tests for thresholds of fire failure (Ref. 10).

In the case of internal flooding, we find that a limited amount of flood initiator data exists in the form of flange leakage frequencies and pipe break frequencies. These are augmented by theoretical studies on the frequency of pipe break which include distributions of crack sizes, rates of crack growth, and degradation due to stress corrosion. Very few data exist on the susceptibility of equipment to either dense steam environments or to flooding. However, in the case of flooding of electromechanical equipment not sealed against water intrusion, it is reasonable and prudent to assume that the equipment will fail by electrical shorting given that it has been submerged. Thus, for flooding, it is reasonable to assume a step function failure probability distribution given submergence and a high correlation between submergence failures.

In the area of harsh environments, data exist on the frequencies of occurrence of the harsh environments in the LER and similar data bases. And similarly, component susceptibilities must be determined from these event report data bases. These data bases are being reexamined under the auspices of the NRC data programs as well as the ongoing common cause failure programs at EPRI. In addition recent, changes in the event reporting requirements will increase the usefulness of data collected in the future.

## II. A UNIFIED APPROACH TO COMMON CAUSE AND EXTERNAL EVENT ANALYSES

The approaches to the analysis of common cause and external event analyses and external events have been shown to be quite similar, each utilizing the steps:

- identify regions of influence (zones)
- evaluate probability of barrier failure
- estimate responses (temperature, vibration level, etc.) and uncertainty
- estimate fragility relations and uncertainty
- estimate correlation coefficient or use equivalent factors
- quantify the cut sets

In performing these six general steps, there are a number of points where screening activities can be performed to minimize the total labor of the analysis. Table 4 illustrates a general dependent failure analysis methodology which has been developed and which provides for several levels of screening. This general screening methodology is discussed in the following paper entitled "A General Screening Methodology for Dependent Failures," by David Campbell, JBF Associates, who was the prime subcontractor to the Dependent Failure Methodology project at Sandia National Laboratories. The reader is referred to that paper for a detailed description of this general methodology and its several variants depending on the type of common cause being considered. It should be noted that this general methodology applies to the full range of common cause failures which must be considered including, for example, human reliability and design defect common cause failures. In this paper, we focus on how the use of multi-stage screening can minimize labor and maximize the efficiency of the interaction between the various experts involved in the analysis of external events and harsh environments. Figure 4 schematically illustrates the general methodology and segregates the

GENERAL DEPENDENT FAILURE ANALYSIS METHODOLOGY

1. IDENTIFY CONDITIONS/ENVIRONMENTS OF INTEREST.
2. IDENTIFY ENVIRONMENTS TO WHICH COMPONENTS ARE SUSCEPTIBLE.
3. IDENTIFY COMPONENT LOCATIONS.
4. DETERMINE ENVIRONMENT/LOCATION COMBINATIONS (CUT SETS) FOR EACH SYSTEM.
5. SCREEN ENVIRONMENT/LOCATION CUT SETS ON HAZARD POTENTIAL.
6. IDENTIFY ACCIDENT SEQUENCES TO BE ANALYZED.
7. DEVELOP ENVIRONMENT/LOCATION CUT SETS FOR EACH ACCIDENT SEQUENCE.
8. SCREEN ACCIDENT SEQUENCE ENVIRONMENT/LOCATION CUT SETS ON HAZARD PROBABILITY.
9. DEVELOP COMPONENT CUT SETS FOR EACH ACCIDENT SEQUENCE.
10. SCREEN ON SCENARIO HAZARD PROBABILITY
11. QUANTIFY FINAL COMPONENT CUT SETS, IF DESIRED.

Table 4

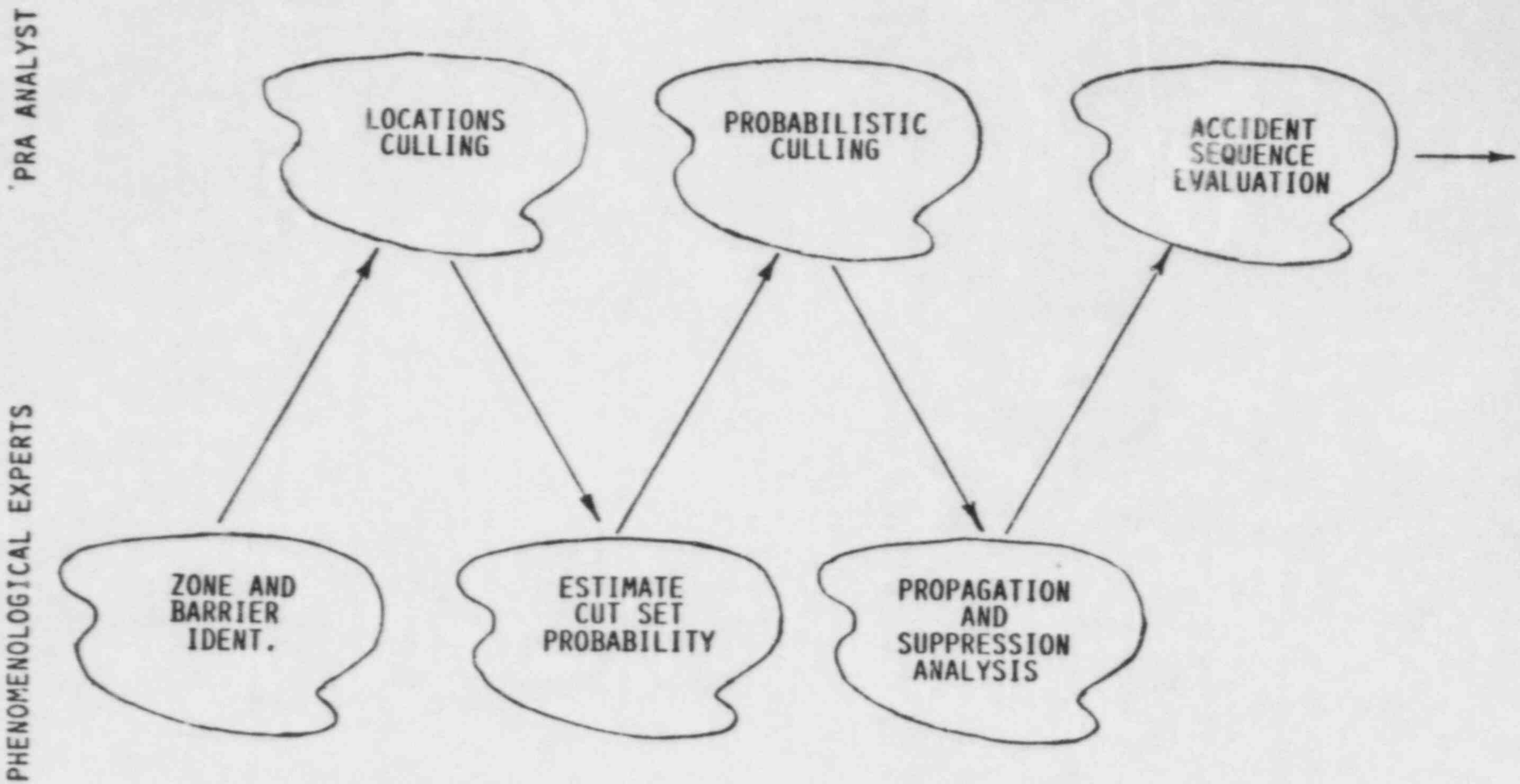


Figure 4. Schematic of Multi-Step Screening Procedure for Common Mode Failures and External Events

activities of the phenomenological experts (fire analysts, earthquake specialists, etc.) and the PRA analyst performing the mainline PRA, and who is responsible for integrating all the various subanalyses which feed the complete PRA.

The analysis begins with the identification of zones and barriers by the phenomenological expert. Once the zones are identified, the components residing in each zone are identified along with their susceptibility to the harsh environments under consideration. Boolean transformations or their equivalent are used to map the accident sequences (in terms of component failures) to accident sequences in terms of zones and random failures of components. These transformed accident sequences can then be culled to exclude all cut sets except those involving, for example, single zones, double zones, or single zones plus random failures. This first culling step significantly reduces the number of zones in the plant which must be considered further.

Once the important zones have been identified, the phenomenological expert makes an estimate of the cut set probabilities which occur due to failures in the identified zones. At this point, it is merely an estimate based on (perhaps) the probability of the occurrence of the harsh environment or based on bounding probabilities for the failure of components. The cut set probability estimates are then submitted to the PRA analyst. Using these bounding values, he performs a probabilistic culling on the accident sequences, discarding those cut sets whose contributions are below some pre-set level of significance. Typically, this level of significance is chosen to be the same as that used in the internal event analyses. This results in deleting a large number of additional cut sets from further consideration.

The final list of significant cut sets is then returned to the phenomenological expert for detailed quantification. It is at this point that computer codes are used to calculate best estimate responses, and best estimate failure criteria are used for the components in the significant cut sets. This is the first point at which numerical calculations are performed in this multi-step process. Further, it is at this point where correlation must be properly taken into account. After the cut sets have been numerically evaluated, their numerical values are returned to the PRA analyst who then incorporates them in his overall accident sequence evaluation. As outlined above, this procedure provides a uniform and consistent level of including dependent failures and external events in the mainline PRA. Further, it optimizes the participation of phenomenological experts into the entire process and clearly defines their level of responsibility.

As noted above, zone screening can remove a significant number of plant zones from further consideration. To illustrate this, consider the zonation shown in Figure 5. This consists of three zones, each zone having two components within the zone which are assumed to be susceptible to the harsh environment under consideration. Assume that the accident sequence in question is given by Equation 6.

$$\text{ACC SEQ} = ABC + BCD + EF \qquad (6)$$

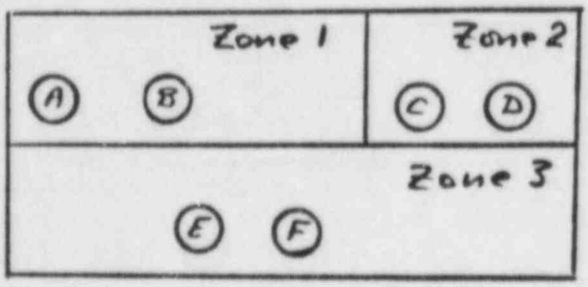


Figure 5. Simplified Zonation Model

A mapping is performed in which each component failure is mapped (in a Boolean sense) to its random failure plus the zone in which it is located. Thus, each of the six components are transformed as shown below:

$$
\begin{aligned}
A &= A + Z1 \\
B &= B + Z1 \\
C &= C + Z2 \\
D &= D + Z2 \\
E &= E + Z3 \\
F &= F + Z3
\end{aligned}
\tag{7}
$$

These are then substituted into the accident sequence (Equation 6) and the rules of Boolean algebra are used to reduce the equation to its simplest form, with the result.

$$
\text{ACC SEQ} = Z3 + Z1{*}Z2 + Z1{*}C + Z2{*}B \tag{8}
$$

It can be seen that this accident sequence has been transformed into the union of four cut sets. The first cut set, Z3, is a single zone cut set, which implies that if the hazardous agent occurs in that zone, the accident sequence will occur. The second cut set, Z1*Z2, is a double zone cut set. This implies that the hazardous agent must occur in both zones simultaneously for the accident sequence to occur. For most harsh environments, the occurrence in both zones simultaneously is highly unlikely.. However, in the case of fire, for example, it is possible that the barrier between zones 1 and 2 could break down under sufficient heat load, in which case the two zones could reduce the one and the double cut set would become a significant contributor to the accident sequence. Hence, the double zone cut sets identify zones which should be further examined to see whether or not they are adjacent and whether or not there are barriers between them which could break down. The third and fourth cut sets are zones in conjunction with random failures. These cut sets identify cases where the combination of the harsh environment in one zone in conjunction with the random failure elsewhere in the plant can cause the accident sequence. Here again the significance of these cut sets is determined when probabilistic culling is applied and the result is compared with the level of significance used in the internal event analyses.

The mapping procedure described above has been automated. Both the COMCAN III (Ref. 11) and SETS (Ref. 12) computer codes are available for performing the transformation of accident sequences to zone cut sets. In the course of the development work in the Dependent Failure Methodology program, we have tested both these computer codes against accident sequences of the size developed and used in the IREP risk analyses. The particular sequer es were a modified version of those used in the IREP ANO-1 PRA (Ref. 13). The particular common causes a) high temperature, and b) vibration were selected for analysis as they were deemed most likely to produce a significant list of common cause candidates. The system fault trees analyzed consisted of up to 1500 basic events and four safety systems. The component list involved 95 different types of components. In addition to determining the three classes of location cut sets described above, the computer codes identified the component minimal cut sets associated with the critical locations. For example, in one of the important transient sequences, the effects of abnormally high ambient temperature in four single locations (involving 24 component cut sets) were found to potentially cause core damage. Similarly, high ambient temperature in six zones in conjunction with 74 random failures (involving a total of 470 component cut sets) was found to potentially cause core damage. Also, for high temperature, five important double zones were identified. Similar results were found for the vibration hazard.

Significant findings from these demonstration calculations were that:

a. The use of location screening on the complete fault trees is quite feasible provided one utilizes a computer code with efficient storage and processing algorithms and which makes use of independent subtree substructuring. The significance here is that the original fault trees can be used rather than trees arrived at through some probabilistic culling which may have discarded many of the important common cause sets during the culling process.

b. For a typical plant of modern design, one can expect to identify on the order of ten important single and double location zones for a single harsh environment and up to ten single location zones which in conjunction with a random (test, maintenance, or operator induced) failure elsewhere in the plant can result in core damage.

c. The importance of including random failures explicitly in conjunction with common cause location-dependent failures was highlighted. For example, a number of the 500 component cut sets identified for this example problem involved a zone plus a random failure which was in no way susceptible to any other common cause hazard identified for the plant and thus which would not have been found in any purely location-dependent culling process.

Thus, our tests of Boolean transformations and location culling as a tool for reducing the effort required to perform dependent failure analyses have demonstrated both the value, and the feasibility of the process. In addition, we find that the Appendix R plant fire submittals usually provide an adequate level of zone breakdown within the plant and should be used as the initial zonation model. Our recommendations are that one should keep at least single zones, single zones plus single random failures, and double zones in performing the location culling procedure.

An important point in evaluating the accident sequences is that one must explicitly include system successes when mapping component failures to zones. When system successes are included, the number of remaining component cut sets which can cause the accident sequences is generally found to drop almost an order of magnitude over those surviving location culling on a safety system level.

The use of the qualitative culling procedure discussed above yields many valuable intermediate products prior to final quantification of the cut sets. For example, on a system level, one identifies those combinations of harsh environments and locations which could result in core damage. This provides insight and direction in evaluating the design of safety systems relative to isolation of crucial components. At the accident sequence level, harsh environment location cut sets are identified which could potentially cause core damage. The component cut sets which could affect the accident sequences are explicitly identified and the component cut sets which survive the probabilistic culling are thus known to be important to a given level of significance. This information can be used by the plant owner/operator as a guide in focusing his reliability assurance program to minimize possible hazards within his plant. For example, the resulting significant cut sets could be binned in three ways:

1. Those component groups whose failure has been shown to be an insignificant contributor to total plant risk. These components would be subject to routine maintenance keyed to the efficient operation of the plant.

2. Those components groups which are potentially significant contributors to the plant risk but which could be included in an upgraded reliability assurance program which would be aimed at guaranteeing that the identified failures could not occur. Thus, by their identification, their contribution to plant risk could be either minimized or eliminated.

3. Those component groups which are potentially significant contributors to the plant risk, but whose contribution to risk could not be further minimized by enhanced reliability assurance procedures. The plant owner/operator, in conjunction with the Nuclear Regulatory Commission, would then assess the significance of these components as to whether or not their contribution could be eliminated by changes in system design or whether the component's risk contribution was consistent with that at other plants and hence would not require further consideration.

Thus, it can be seen that this uniform and consistent approach to including common cause failures, harsh environments and external events provides a means of systematically evaluating the various contributions to the overall plant risk and provides tools (both quantitative and qualitative) to allow both the plant operator and the NRC to assess the potential risk at the plant and the potential means of eliminating or minimizing this risk.

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# AN NRC APPROACH TO DEPENDENT FAILURE ANALYSIS

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The risk contributions from redundant safety systems in nuclear power plants are often dominated by dependent failure scenarios. A dependent failure scenario is one in which multiple components fail due to the same root cause. These scenarios result from harsh environments affecting a group of components or from human errors that are systematically repeated among a group of components. A set of procedures for identifying, screening, and analyzing dependent failure scenarios has been developed. These procedures are compatible with existing dependent failure analysis computer programs. The procedures were used to analyze the dependent failure scenarios associated with selected accident sequences from the Arkansas Nuclear One - Unit 1 PRA. The results of this demonstration application show that use of the screening steps in the procedures makes the qualitative portion of the analysis easily manageable.

## INTRODUCTION

The defense-in-depth concept, when applied to the design of nuclear power plant safety systems, creates a situation in which risk is determined by accidents that cannot occur unless multiple components fail to perform their design functions. The more likely cause of these multiple component failures is a dependent failure. Risks posed by nuclear power plant operation, therefore, are usually dominated by dependent failure scenarios. This statement is supported by the results of probabilistic risk assessments (PRAs) and historical experience of nuclear power plant operation.

A dependent failure scenario is defined as a scenario in which two or more components fail due to the same cause. The PRA Procedures Guide[1] defines a dependency between two failure events by specifying that a dependency exists whenever the probability of the two events is greater than the product of the individual event probabilities.

This paper describes a set of procedures for performing a dependent failure analysis as part of a PRA. The procedures presented in this paper focus primarily on qualitative insights gained while performing dependent failure analysis. Emphasis is on qualitative screening techniques that can be used to separate unimportant scenarios from potentially important scenarios, thus focusing attention on the areas of greatest interest.

The most important objective of the procedures presented here is to provide the analyst with sufficient information for sorting potential dependent failure scenarios into the following categories:

1. scenarios that are significant contributors to plant risk and warrant specific corrective or preventive action by the plant owner

2. scenarios that are potentially significant contributors to plant risk, but can be controlled or prevented by an effective reliability assurance program

3. scenarios that are insignificant contributors to plant risk

These categories imply that action should be taken to reduce the risk from the scenarios that are the significant contributors to plant risk.

Two parallel sets of procedures are presented. One set of procedures applies to the analysis of dependent failures caused by design, fabrication, installation, maintenance, testing or operating errors (component location-independent causes of dependent failures). These causes of failure can affect multiple components regardless of their relative locations. The other set of procedures applies to dependent failures that are caused by harsh environments (component location-dependent causes of failure). For a harsh environment to cause the failure of group of components, all of the components in the group must be in the same location relative to the harsh environment.

In many cases, the procedural steps can be applied to any type of system reliability or risk analysis that uses fault trees and/or event trees. However, some of the screening steps, and the order in which they are presented, require performing the analysis as part of a PRA which has relatively little detail in the event trees, highly detailed fault trees, and a fault tree analysis of functional systems interactions.

An attractive feature of the procedures is that the more labor-intensive steps can be performed by using existing dependent failure anlaysis software. To illustrate this point, we describe a demonstration of the use of the COMCAN III computer program with these procedures. The results of this application indicate that use of the screening steps in these procedures makes the qualitative portion of a dependent failure analysis easily manageable.

## OVERVIEW OF THE LOCATION-INDEPENDENT DEPENDENT FAILURE ANALYSIS PROCEDURE

This procedure consists of eight steps. Each step will be briefly discussed.

**<u>Step 1</u> - Identify important root causes of component failures and define groups of similar components that are susceptible to the same root causes of failure.**

Nuclear power plant operating experience indicates that most of the root causes of location-independent dependent failures (LIDFs) (errors in design, manufacturing, installation, testing, and maintenance) result in dependent failures of similar components. Thus, for these failure causes, <u>component groups</u> are defined as <u>groups of similar components</u>. For each of these component groups, the analyst identifies susceptibilities to root causes of failure. Historical experience (as described in licensee event reports [LERs]), is the best guide in discovering the susceptibilities of different component groups to particular causes of failure.

**<u>Step 2</u> - Determine the emergency operating procedures (EOPs) that affect each component.**

Operating errors, which can also cause LIDFs, are covered by this step. Any set of components controlled by the same EOP for actuation or realignment must be considered as a candidate for dependent failure at this point in the analysis.

**<u>Step 3</u> - Determine the component group and EOP scenarios that alone can cause safety system failures and those that can cause safety system failures when combined with a single independent failure.**

This step is performed with assistance from a computer program specifically designed to test safety system fault trees. The purpose of the fault tree tests is to see if the top event can occur as a result of the occurrence of a group of component failures. The component groups of interest will be those that contain similar parts or those that are affected by the same EOP. A partial dependent failure scenario is of interest if failures of all components within a group cannot cause a safety system failure by themselves, but can cause a safety system failure in combination with a single independent failure.

**<u>Step 4</u> - Determine the component group and EOP scenarios that alone can cause PRA accident sequences and those that can cause PRA accident sequences when combined with a single independent failure.**

This step is similar to Step 3; however, it is performed manually. The procedure is to observe which system failures can be caused by each component group or EOP scenario and then to assign each scenario to its appropriate accident sequence. As in Step 3, if a complete failure of all systems involved in an accident sequence cannot occur, then partial dependent failure scenarios (dependent failures that combine with a single independent failure to cause an accident sequence) would be of interest.

Step 5 - **Determine the component minimal cut sets that are involved in each component group and EOP scenario.**

This step uses a computer program designed for common cause or dependent failure analysis. Each accident sequence fault tree is solved for any scenario that can cause the sequence to determine the minimal cut sets involved.

Step 6 - **For each cut set identified in Step 5, identify specific installation, testing, and maintenance procedures that affect all components. Also identify the cut sets whose components share a common manufacturer.**

This step screens out cut sets with components that are not subject to the same procedures during plant operation or with components that do not share a common manufacturer. Those cut sets thus screened out are probabilistically less important than those that remain.

Step 7 - **Screen all remaining scenarios by considering details of the relationships between the root causes of failure and the component failures in the cut sets.**

This step involves a detailed review of procedures to identify specific human errors triggering LIDF scenarios. In addition, the analyst evaluates all test staggering policies and the adequacy of operational checks applied after maintenance or after a change in a component's status. (Many of the remaining scenarios could be eliminated by this screening step if the plant has a good reliability assurance program.)

Step 8 - **Quantify the remaining dependent failure scenarios.**

Different quantitative models apply to analysis of different root causes of failure. Human performance reliability models can be used to predict error probabilities in operating, maintenance, and testing activities related to plant operations. We investigated the use of the Maintenance Personnel Performance Simulation (MAPPS) model to generate error probabilities for both testing and maintenance tasks. However, MAPPS does not model the correlation between successive performances of the same task; we therefore developed a dependence model to supplement it. Operating error susceptibility could be analyzed by using any of the techniques applied in past human reliability analyses for operator response modeling (e.g., the Technique for Human Error Rate Prediction, or the Operator Action Tree System).

Errors in design and fabrication are not related to plant operations. Thus, the aforementioned models are of no use in analyzing these types of errors. Since little is known about these failure causes, it is judicious to quantify them directly through the use of operating experience data. This may be accomplished by applying a parametric model (e.g., $\beta$-factor or BFR) using root-cause-specific parameters. Even though no parameters of this type are reported in the literature, it is possible to estimate them in some cases.

## OVERVIEW OF THE HARSH ENVIRONMENT DEPENDENT FAILURE ANALYSIS PROCEDURE

This procedure consists of nine steps. Each step will be briefly discussed.

**Step 1 - Identify important root causes of component failures and component susceptibilities to those root causes.**

Historical experience, as described in LERs, is the best guide in determining what types of environments have caused multiple component failures and what environments are most likely to cause individual types of components to fail.

**Step 2 - Identify locations of components and barriers to the propagation of harsh environments and develop domains.**

In this step, the analyst locates the components identified in the PRA fault trees. Exact locations are not necessary at this point in the analysis; it is only necessary to show the room (or rooms) containing each component. This part of Step 2 can usually be accomplished without a plant visit.

Identifying barriers to harsh environments may, however, require a plant visit since barriers to one environment may not be barriers to another. Safety analysis reports describe fire barriers and flood zones within plants. This information can be used for preliminary identification of barriers to other harsh environments, or the analyst can visit the plant and obtain detailed barrier descriptions for each environment of interest.

Component and barrier locations are used to develop domains for the harsh environments of interest. A domain is an area within a plant that is bounded by barriers to a particular harsh environment.

**Step 3 - Determine the harsh environment scenarios that alone can cause safety system failures and those that can cause safety system failures when combined with a single independent failure.**

This step is similar to Step 3 in the LIDF Analysis Procedure. The only difference is that the groups of component failures to be tested with computer assistance are defined so that: (1) all of the components must be susceptible to the same harsh environment, and (2) all must be in the same location relative to the environment. Here, as in the analysis of LIDFs, any partial dependent failure scenario may be of interest.

**Step 4 - Screen the harsh environment scenarios identified in Step 3 based on the potential for a root cause event that triggers the scenario.**

It is cost effective at this point in the analysis to determine whether a credible source of a harsh environment exists in those locations identified in Step 3 as potentially significant. If no credible source exists, the scenario can be discarded from further consideration.

**Step 5 - Determine the harsh environment scenarios that can alone cause PRA accident sequences and those that can cause PRA accident sequences when combined with a single independent failure.**

This step is identical to Step 4 in the LIDF Analysis Procedure.

**Step 6 - Screen the harsh environment scenarios identified in Step 5 based on the likelihood of root cause events that trigger the scenarios.**

The analyst now knows which accident sequence each remaining harsh environment scenario can cause. Based on a knowledge of other contributions to the frequency of the sequence (e.g., the independent failure contribution), it may be possible to discard some scenarios from further consideration if the frequency of the root cause event resulting in the harsh environment is low compared to other contributions.

**Step 7 - Determine the component minimal cut sets that are involved in each remaining harsh environment scenario.**

This step is similar to Step 5 in the LIDF Analysis Procedure; however, the definition of the component groups involved in each scenario differs. Here, as in Step 3, components (1) must be susceptible to the same harsh environment, and (2) must be in the same location relative to the environment if they are considered to be in the same group.

**Step 8 - Screen all remaining scenarios by considering details of the relationships between the root causes of failure and the component failures in the cut sets.**

The analyst now has a complete qualitative description of the root cause event and the components that must fail for each scenario to occur. A plant visit is required at this point to make a detailed survey of: (1) spatial relationships of components, (2) root cause sources, and (3) barriers. Some scenarios may not be credible in light of the findings. For those scenarios that are credible, such findings are necessary for the frequency evaluation conducted in Step 9.

**Step 9 - Quantify the remaining dependent failure scenarios.**

The stress-strength interference model (SSM) is the most useful model available for quantifying harsh environment scenarios or for performing sensitivity studies on scenario frequencies. The focus of the SSM application is to gain insights useful for screening purposes. The SSM provides modeling and statistical inference for the reliability of components and systems. The method is based on the concept that a given component has a stress-resisting capacity or strength. A component failure results whenever the environmentally induced stress exceeds the strength of the component. Failure may occur when the stress surpasses the component's design envelope. Failure may come from a lowering of the component's strength to a level below its design-basis stress. Or the failure may come from a combination of the two causes. The terms stress and strength apply here to any environment that induces failure, such as high temperature or high vibration conditions. Application of the SSM requires the analyst to characterize the correlation among the several variables due to the interdependencies among the variables. A correlation may exist among stress variables, among strength variables, and among stress and strength variables.

As an example of stress and strength variables, consider an air conditioning system failure that results in a high temperature in a specific location. In this case, the stress is the new temperature in the location following the occurrence of the failure event. The exact temperature at each component location depends on several factors, including the location of the heat sources and the time to detect the failure and repair the air conditioning system. Therefore, the stress imposed on each component is actually a random variable characterized by an average value and a standard deviation. Similarly, the strength of each component is also a random variable characterized by a mean value and a standard deviation.

## DEMONSTRATION APPLICATION

Currently available probabilistic risk assessment (PRA) computer programs can be used to perform those steps of the dependent failure analysis (DFA) procedures requiring computer aid. We demonstrated this point by using the COMCAN III computer program to perform certain steps of the procedures on selected accident sequences from the Arkansas Nuclear One-Unit 1 (ANO-1) PRA.

Only those DFA steps requiring computer aid are discussed in this paper, and only a few ANO-1 accident sequences are analyzed to demonstrate these steps. (Results of other DFA steps required to perform the computer-aided steps were either assumed by the analysts or taken from previous DFA studies on ANO-1.)

In performing the DFA demonstration, we considered dependent failures that result in partial or total occurrence of the selected accident sequences. The dependent failures considered can result either from:

- design, fabrication, installation, maintenance, or operating errors (component location-independent causes of failure) or

- harsh environments such as high temperature moisture, vibration, and impact (component location-dependent causes of failure)

## Technical Data Sources

Sandia Laboratories completed and published an IREP study on the ANO-1 Plant.[2] The results of the study were used as the primary technical information base for this demonstration analysis. Specifically, the following IREP information was used to formulate the basis for this DFA of selected ANO-1 accident sequences:

1. a general plant description
2. accident sequence event trees
3. initiating event and accident sequence definitions
4. system descriptions
5. system fault tree models
6. event/subevent definition tables

In addition to the above data, several computer models and data files developed by Sandia during the ANO-1 IREP study were used. Sandia and NRC personnel have made some minor modifications to these models and files since publication of the ANO-1 IREP; we used their modified files in this demonstration application.

Other information sources used in this demonstration application were:

1. licensee event reports (LERs) for component susceptibility and root cause identification
2. plant visit for information on the operating environment, plant layout, and equipment locations
3. plant system drawings for details of equipment configurations

## Accident Sequence Selection

The accident sequences selected for dependent failure analysis may be defined as:

**Sequence $T(A3)LD_1$** - a transient initiated by failure of the engineered safeguards bus A3 [T(A3)] followed by failures of the Emergency Feedwater System (EFS or L) and the feed and bleed mode of the High Pressure Injection System (HPIS or $D_1$)

**Sequence $T(A3)LD_1C$** - the initiating event T(A3) followed by failure of the EFS(L), HPIS($D_1$), and Reactor Building Spray Injection System (RBSI or C)

**Sequence $T(A3)LD_1Y$** - the initiating event T(A3) followed by failures of the EFS(L), HPIS($D_1$), and Reactor Building Cooling System (RBCS or Y)

**Sequence** $T(A3)LD_1YC$ - the initiating event T(A3) followed by failures of the EFS(L), HPIS($D_1$), RBCS(Y), and RBSI(C)

Each front-line system identified in the accident sequences requires system support for successful operation. Failures of the following support systems are included in the appropriate front-line system fault trees:

1. Engineered Safeguards Actuation System (ESAS)
2. Service Water System (SWS)
3. Emergency AC Power System (EACPS)
4. DC Power System (DCPS)
5. Battery and Switchgear Emergency Room Cooling System (ECS)
6. Emergency Feedwater Initiation and Control System, Initiation Subsystem and Vector Subsystem (EFIC-I/EFIC-V)

## Demonstration Application of COMCAN III with the Location-Independent Dependent Failure Analysis Procedure

**Step 1 - Identify important root causes of component failures and define groups of similar components that are susceptible to the same root causes of failure.**

Important location-independent root causes of failure were identified in a separate study. That study involved the review and classification of more than 300 Licensee Event Reports (LERs). The study identified six important location-independent failure causes: (1) design, (2) installation, (3) maintenance, (4) manufacturing, (5) operating, and (6) testing errors.

The component groups selected for this analysis are: motor-driven pumps, pneumatic valves, turbine-driven pumps, diesel generators, and refrigeration units.

**Step 2 - Determine the emergency operating procedures (EOPs) that affect each component.**

These scenarios are identified and screened using the same methods that apply to scenarios involving similar components. For this demonstration application, we did not consider potential EOP scenarios.

**Step 3 - Determine the component group and EOP scenarios that alone can cause safety sytem failures and those that can cause safety system failures when combined with a single independent failure.**

The information required for completing this analysis step consists of the system fault trees, the similar component groups defined in Step 1, and the list of EOPs defined in Step 2 that affect each basic event. We used the COMCAN III computer program to test the system fault trees.

We tested each system fault tree for each component group. Table 1 summarizes the results of these tests. The results for the transient initiating event [T(A3)] were obtained without computer aid. We identified the component groups associated with this event by inspection.

In Table 1 an entry of "Yes" indicates that the component group affects all basic events in one or more minimal cut sets (MCSs) for the system. An entry of "Partial" denotes the existence of one or more partial LIDF scenarios involving the specified component group and system. An entry of "No" implies that LIDF scenarios, partial or complete, involving the component group do not exist for that system.

Table 1. Results of System Fault Tree Tests on Component Groups

| IE or System \ Comp. Group | Pneumatic Valves (AV) | Motor-Driven Pumps (MP) | Turbine-Driven Pumps (TP) | Diesel Generators (DG) | Refrigeration Units (RU) |
|---|---|---|---|---|---|
| T(Δ3) | No | No | No | No | No |
| EFS | Partial | Yes | Partial | Partial | No |
| HPIS (1 of 3) | No | Yes | No | Partial | No |
| RBCS | No | Yes | No | Partial | No |
| RBSI | No | Yes | No | Partial | No |

**Step 4 - Determine the component group and EOP scenarios that alone can cause PRA accident sequences and those that can cause PRA accident sequences when combined with a single independent failure.**

We used the results of testing system fault trees in Step 3 to identify potential LIDF scenarios at the accident sequence level. The results of the system fault tree tests were compared with each sequence to determine if any potential LIDF scenarios (partial or complete) exist for that sequence.

Once the accident sequence assignments were made, we placed each scenario into one of four categories:

Type 1: scenarios in which the root cause event can cause every basic event in an MCS to fail, including the accident initiating event

Type 2: scenarios in which the root cause event can cause all basic events in an MCS to fail, except for the accident initiating event

Type 3: scenarios in which the root cause event can cause the accident initiating event and all but one of the remaining basic events in an MCS to fail

Type 4: scenarios in which the root cause event can cause all basic events in an MCS to fail, except for the accident initiating event and one other basic event

We used the matrix completed in Step 3 to identify potential LIDF scenarios at the accident sequence level. By reading down the column corresponding to a particular component group, we determined the most severe accident sequence for which there are complete or partial LIDF scenarios involving that component group. For example, in Table 1 we find that complete LIDF scenarios involving component group MP exist for the EFS, HPIS, RBCS, and RBSI. Thus, we know that one or more complete dependent failure scenarios exist for the accident sequence $T(A3)LD_1YC$. Further, since the initiating event does not involve this component group, we can say that the scenarios are Type 2.

As a final example, consider refrigeration units. Table 1 shows that there are no LIDF scenarios involving this component group for any system. Therefore, we screened refrigeration units from the analysis.

We applied similar reasoning in analyzing the other component groups. Table 2 shows the results of this analysis step. We screened out three component groups in this step. Component Groups AV and TP were screened out because the sequence that they cause [T(A3)L] does not result in core melt.

Table 2. Potential LIDF Scenarios

| Component Group or EOP Code | Accident Sequence Assignment | Potential LIDF Scenarios | | | |
|---|---|---|---|---|---|
| | | Type 1 | Type 2 | Type 3 | Type 4 |
| AV | T(A3)L screen out | | | | |
| MP | T(A3)$LD_1$YC | N | Y | N | N |
| TP | T(A3)L screen out | | | | |
| DG | T(A3)$LD_1$YC | N | N | N | Y |
| RU | screen out | | | | |

**Step 5 - Determine the component minimal cut sets that are involved in each component group and EOP scenario.**

The information required for this step consists of the accident sequence fault trees and the list of potential LIDF scenarios for each component type that passed screening in Step 4. By using this information and computer aid, we determined the minimal cut sets (MCSs) that are involved in each component group scenario.

To show the use of COMCAN III in obtaining MCSs for both complete and partial LIDF scenarios, we determined the MCSs for two of the scenarios identified in Step 4: the Component Group MP with sequence T(A3)$LD_1$YC (MP/T(A3)$LD_1$YC), and the Component Group DG and the independent loss of offsite power with sequence T(A3)$LD_1$YC (DG/LOSPOWER/T(A3)$LD_1$YC). For MP/T(A3)$LD_1$YC we found a total of 9 MCSs. Each of these MCSs involves failures of redundant service water pumps. For DG/LOSPOWER/T(A3)$LD_1$YC we found one MCS.

We did not perform Steps 6-8 of the LIDF analysis procedure as a part of this demonstration application. They are performed manually.

## Demonstration Application of COMCAN III with the Harsh Environment Dependent Failure Analysis Procedure

**Step 1 - Identify important root causes of component failures and component susceptibilities to those root causes.**

The most important types of harsh-environment-related dependent failures were identified in an earlier study. The results of that study are based on a review of over 200 Licensee Event Reports (LERs). The review showed that the following types of harsh environments are most likely to cause dependent failures: impact, moisture, high temperature, and vibration.

**Step 2 - Identify locations of components and barriers to the propagation of harsh environments and develop domains.**

The locations associated with each basic event from the ANO-1 fault trees and domain maps for the important harsh environments (defined in Step 1) were developed from plant tours, review of plant layout drawings, and discussions with plant personnel.

**Step 3 - Determine the harsh environment scenarios that alone can cause safety system failures and those that can cause safety system failures when combined with a single independent failure.**

We used COMCAN III to perform the fault tree tests. Three types of information are required by COMCAN III for analyzing harsh environments: (1) a fault tree in coded format, (2) location and susceptibility data for the basic events, and (3) domain definitions.

To demonstrate this procedural step, we tested the system fault trees for each of 21 vibration domains. The results from some of these tests are shown in Table 3.

**Step 4 - Screen the harsh environment scenarios identified in Step 3 based on the potential for a root cause event that triggers the scenario.**

We did not identify specific root cause events for this analysis. The reasoning is that it would not help demonstrate the use of COMCAN III in performing a dependent failure analysis.

**Step 5 - Determine the harsh environment scenarios that alone can cause PRA accident sequences and those that can cause PRA accident sequences when combined with a single independent failure.**

The results of the system fault tree tests (Step 3) were used to identify potential dependent failure scenarios caused by harsh environments for each accident sequence. Each scenario was placed into one of the four categories discussed earlier.

Table 3. Sample Results of System Fault Tree Tests on Vibration Domains

| IE or System \ Domain | VV-01 | VV-02 | VV-04 | VV-05 | VV-08 | VV-10 | VV-15 | VV-18 |
|---|---|---|---|---|---|---|---|---|
| T(A3) | No | No | No | No | No | No | No | No |
| EFS | No | No | No | Yes | Partial | Partial | Partial | Yes |
| HPIS (1 of 3) | No | No | Yes | Yes | Yes | Partial | Partial | Yes |
| RBCS | No | No | No | Yes | Yes | Partial | Partial | Yes |
| RBSI | Partial | Partial | Yes | Yes | Yes | Partial | Partial | Yes |

By using the matrix completed in Step 3, we assigned each vibration domain to the most severe accident sequence for which there are potential dependent failure scenarios involving that domain. The procedure for performing this step is identical to the one used in Step 4 of the LIDF analysis procedure. Table 4 shows the results of this step. Note that domains VV-01 and VV-02 were screened from the analysis because the accident sequence that they cause [T(A3)C] does not result in core melt.

Table 6. Vibration Scenarios

| Vibration Domain | Accident Sequence Assignment | Vibration Scenarios | | | |
|---|---|---|---|---|---|
| | | Type 1 | Type 2 | Type 3 | Type 4 |
| VV-01 | T(A3)C (screen out) | | | | |
| VV-02 | T(A3)C (screen out) | | | | |
| VV-04 | $T(A3)D_1C$ | N | Y | N | N |
| VV-05 | $T(A3)LD_1YC$ | N | Y | N | N |
| VV-08 | $T(A3)LD_1YC$ | N | N | N | Y |
| VV-10 | $T(A3)LD_1YC$ | N | N | N | Y |
| VV-15 | $T(A3)LD_1YC$ | N | N | N | Y |
| VV-18 | $T(A3)LD_1YC$ | N | Y | N | N |

**Step 6 - Screen the harsh environment scenarios identified in Step 5 based on the likelihood of root cause events that trigger the scenarios.**

We did not perform Step 6 in this demonstration. It is performed manually.

**Step 7 - Determine the component minimal cut sets that are involved in each remaining harsh environment scenario.**

To demonstrate the use of COMCAN III in finding MCSs for dependent failure scenarios involving harsh environments, we determined the MCSs for two vibration scenarios: Domain VV-18 with accident sequence $T(A3)LD_1YC$ ($VV\text{-}18/T(A3)LD_1YC$), and Domain VV-08 and the independent failure of event ACBSA4NP (loss of power from 4160V AC bus A4) with accident sequence $T(A3)LD_1YC$ ($VV\text{-}08/ACBSA4NP/T(A3)LD_1YC$). For $VV\text{-}18/T(A3)LD_1YC$ we found 40 MCSs, all of which involve failures of service water equipment. For $VV\text{-}08/ACBSA4NP/T(A3)LD_1YC$ we found 12 MCSs. Unlike the other scenarios we analyzed, $VV\text{-}08/ACBSA4NP/T(A3)LD_1YC$ is not dominated by support system failures.

We did not perform Steps 8 and 9 in this demonstration. These steps are performed manually.

## PROJECT STATUS

The initial demonstration applications of the dependent failure analysis procedures show that the screening steps in the procedures make the analysis manageable. By using these screening steps along with the COMCAN III computer program, we were able to solve several PRA accident sequences for dependent failure scenarios.

Future research will focus on quantitative modeling of dependent failure scenarios and on refining the analysis procedures. The final product of our research will be a procedures guide for performing dependent failure analyses. The guide will be published early in 1986.

## REFERENCES


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PRA PROCEDURES FOR DEPENDENT EVENTS ANALYSIS--
AN INDUSTRY PERSPECTIVE

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## INTRODUCTION

There is a wide degree of consensus within the risk, reliability, and nuclear safety communities that dependent events:

- Have been principal contributors to experienced nuclear power plant accidents and incidents in which safety systems have failed or have performed below expectations.
- Have been identified as major contributors to risk and system unavailability and unreliability in numerous applied risk and reliability studies.
- Still represent a major source of confusion, inconsistency, controversy, and analytical variability in recent and current risk and reliability assessments.
- Deserve a high priority in the development of systematic procedures for their incorporation into risk and reliability assessments.

Statements such as these have inspired and guided current research projects at the Electric Power Research Insititue (EPRI) in the field of dependent events analysis. The recognition of a need for more systematic procedures in this area is shared by many and is evidenced by the existence of a number of parallel efforts in the U.S. and abroad to develop such procedures. In addition to the procedures development effort at EPRI (Reference 1), separate projects to refine the approach to dependent events analysis are underway in the Risk Methods Integration and Evaluation Program (RMIEP) of the Nuclear Regulatory Commission (NRC) (Reference 2) and in Europe. It is hoped that through coordination of these parallel efforts, we may eventually achieve a consensus technical approach to dependent events analysis, as illustrated in Figure 1.

The particular focus of this paper is the nuclear industry perspective on dependent events analysis and the progress made thus far in the development of systematic procedures that are needed for industry-sponsored risk and reliability evaluations. While this particular perspective is indeed appropriate for a project sponsored by the Electric Power Research Institute, the principal objectives are no different than those of parallel efforts in the NRC and European programs: the achievement of a better understanding of the risks, causes, and defenses against dependent events.

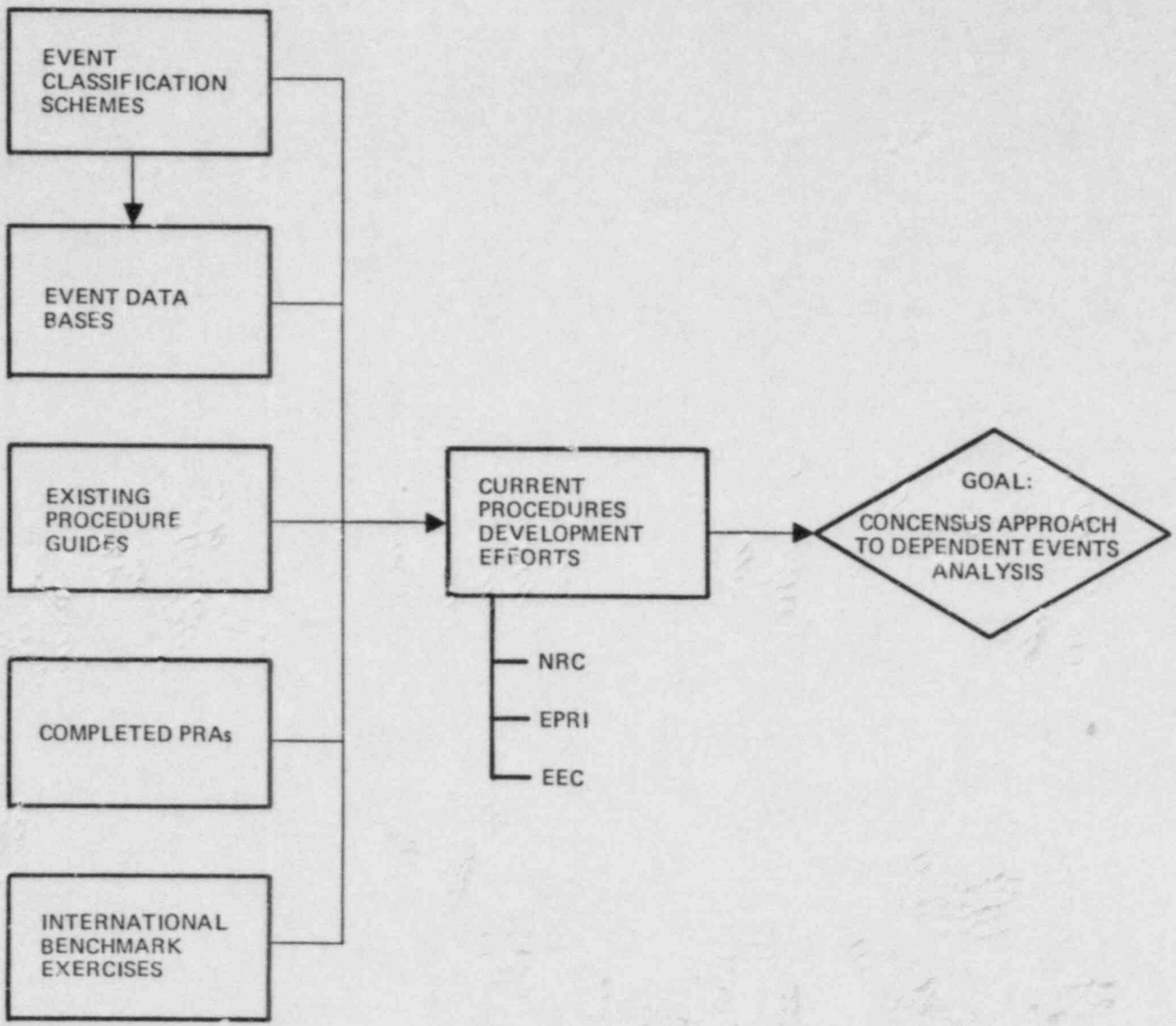


FIGURE 1. DEVELOPMENT OF CONSENSUS APPROACH TO DEPENDENT EVENTS ANALYSIS

## DEFINITIONS AND TAXONOMY

In order to describe the procedures developed for dependent events analysis, it is first helpful to set forth some basic definitions and means of classifying and categorizing dependent events. The purpose here is not to promote the intense controversy that sometimes surrounds the quest for a surgically precise definition, but rather to simply convey an appreciation of the nature and scope of the problems facing the dependent events analyst.

There are two distinct levels of analysis in which dependent events play an important role: the plant level and the system level. The former level is associated with probabilistic risk assessments (PRA) in which large numbers of accident sequences involving the entire plant are modeled. The latter level is also associated with PRAs to the extent that system contributions to accident sequences are modeled. It also applies to system-level applications to assess system performance measures, such as reliability and availability. The dependent events procedures guide described in this paper is being developed and organized with both of these levels and applications in mind.

With this perspective, the starting point for defining and classifying dependent events is the following simple observation. A major portion of plant-level and system-level risk and reliability analysis is concerned with the development and analysis of such logic models as event trees and fault trees. These models depict the logical relationships between the events of interest and concern, such as potential accident sequences and system failure modes, and such subordinate events as combinations of component failure modes or unavailabity causes whose frequencies are known or capable of being estimated from the available evidence. the logic model is simply a tool to enable us to apply the axioms of probability theory, so we can express the probabilities and frequencies of the accident sequences and system failure modes in terms of the more readily quantifiable subordinate events.

Having established that plant and system-level risk and reliability analysis involve an application of probability theory axioms, it is not really necessary to agree on a new definition of dependent events; there is already a well-established one in probability theory. This definition states that the events A and B are independent if, and only if

$$P(A|B) = P(A) \tag{1}$$

$$P(B|A) = P(B) \tag{2}$$

That is, the probability of the occurrence of either event does not depend on, or is in any way affected by, the knowledge that the other event has occured. Hence, it follows that A and B are dependent events when

$$P(A|B) \neq P(A) \tag{3}$$

and

$$P(B|A) \neq P(B) \tag{4}$$

When the events A and B are stochastic events, their so-called "statistical" dependence implies the existence of a physical cause-effect relationship that links the occurrence of the events, at least on occasion. Hence, there is an intimate relationship between the so-called "statistical dependence," and the physical dependence among a set of events.

It is clear that dependent events, as defined above, form a very broad class of events that includes many different types. This creates the potential for a wide variety of ways for their classification and categorization. Indeed, a large amount of this potential has already been realized in the available literature on this subject. The alternative schemes to classify and categorize give rise to numerous "buzzwords" that often refer to a particular subset of dependent events. Some of the better known buzzwords are "common mode failures," "common cause failures," and "systems interactions." Because of the many different types of events involved, attempts to provide sharp, unambiguous definitions for these "buzzwords" have had mixed results, at best. Somewhat greater success has resulted from specific attempts to develop a taxonomy for the broad and all-encompassing notion of a dependent event.

Some of the better known attempts to develop a taxonomy for dependent events (i.e., a systematic, "top-down" categorization scheme for these events) are summarized in Table 1. While there is much in common with the different approaches listed, each provides a unique perspective of the various attributes of dependent events, and, taken as a whole, all have contributed to a better understanding of their nature, causes, and possible defenses.

The categorization scheme of the PRA Procedures Guide provides a convenient way to identify the nature and scope of dependent events analysis in a PRA. The characteristics of this scheme, summarized in Table 2, have proved to be particularly useful in the organization of the new procedures guide. The logic of this categorization scheme is based on the observation that dependent events must be considered not only in the quantification, but also in the definition, of accident sequences in a PRA. Accident sequences are defined by initiating events and event trees. Hence, dependent events can: (1) cause initiating events, (2) interact with two or more events in the event tree, or (3) interact with components within a given event in the event tree. The dependent events categories determined by these three possibilities can then be further subdivided along one or more of the schemes described in Table 1. As illustrated in Figure 2, a given accident sequence may entail the analysis of numerous, separate dependent events in its definition and quantification. This is a major reason why dependent events analysis cannot be considered as a separate task, but rather as an integral part of the analysis tasks in a PRA.

## PRA PROCEDURE FRAMEWORK

Because of the need to support two separate levels of analysis (i.e., the plant-level PRA analysis and the system-level reliability analysis), it was decided to structure the procedures guide for dependent events

TABLE 1. DIFFERENT APPROACHES TO DEPENDENT EVENTS CATEGORIZATION

| Dependent Events Categorization Scheme | Basis of Categorization | Reference |
|---|---|---|
| Edwards and Watson | Hierarchy of engineering and operational activities to identify specific categories of causes. | (3) |
| Generic Cause | Comprehensive set of causes and conditions that lead to dependent events with emphasis on spatial interactions. | (4) |
| PRA Procedures Guide | Categories and subcategories defined by different ways dependent events impact a PRA model. | (5) |
| EPRI Systems Interaction Procedure Guide | Logical breakdown of different types of trigger events and coupling mechanisms that cause the events. | (6) |
| EPRI Event Classification Scheme | Categories based on different key structures of cause-effect logic diagrams developed for experienced events. | (7), (8) |

TABLE 2. TYPES OF DEPENDENT EVENTS BASED ON THEIR IMPACT ON A PRA MODEL

| Dependent Event Type | Characteristics | Subtypes (coupling mechanisms) | Examples (trigger events) |
|---|---|---|---|
| 1. Common Cause Initiating Event | Causes a plant transient and increases unavailability of one or more mitigating systems. | • Functional<br>• Spatial<br>• Human | • Loss of Offsite Power<br>• Earthquake<br>• Maintenance error shorting out instrument bus. |
| 2. Intersystem Dependency | Causes a dependency in a joint event probability involving two or more systems. | • Functional<br>• Spatial<br>• Human | • Coolant charging fails because component cooling fails.<br>• Fire causes loss of equipment two systems.<br>• Operator error causes loss of two systems. |
| 3. Intercomponent (intrasystems) Dependency | Causes a dependency in a joint event probability involving two or more components. | • Functional<br>• Spatial<br>• Human | • Battery loses charge after it is run beyond capacity.<br>• Fire causes loss of redundant pumps.<br>• Design error present in redundant pump controls. |

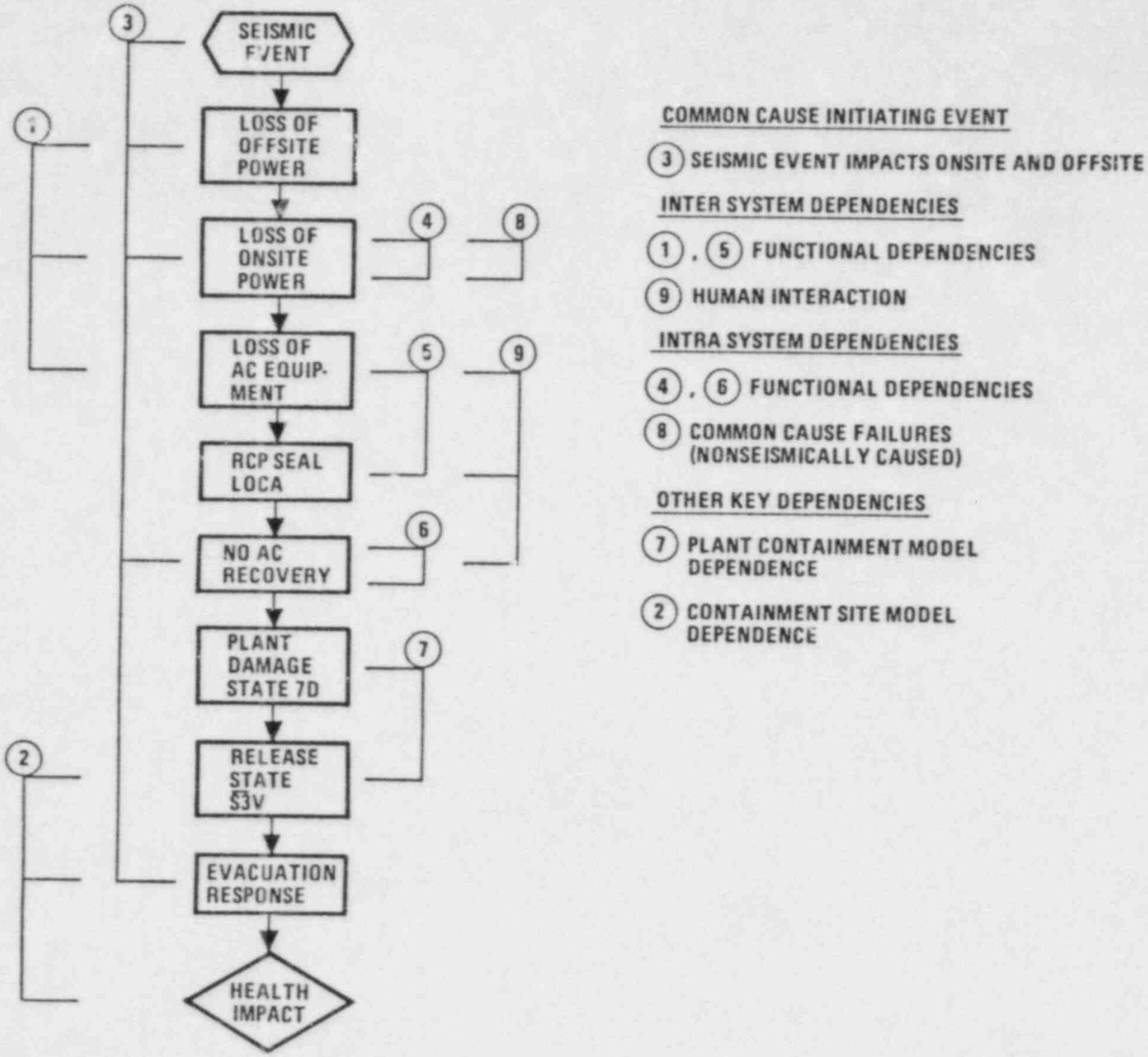


FIGURE 2. SOME KEY DEPENDENCIES IN AN ACCIDENT SEQUENCE

analysis into two volumes. A common framework was identified that seemed to fit both levels of analysis and, therefore, to provide the structure for organizing both volumes. This framework divides the tasks of dependent events analysis into five distinct phases

1. Familiarization
2. Definition
3. Screening (qualitative analysis)
4. Quantification
5. Interpretation

The familiarization of Phase 1 is that of the plant or system under investigation. While this step might seem rather obvious to some, its omission has been the cause of many unsuccessful attempts to perform a competent analysis. Because it is where the bulk of the effort is actually expended, it is useful to provide further definition of the important tasks in Phase 4.

- Task 4.1, Logic Model Development
- Task 4.2, Boolean Reduction
- Task 4.3, Parametric Model Development
- Task 4.4, Data Analysis
- Task 4.5, Point Estimate Analysis
- Task 4.6, Uncertainty Analysis

The specialization of this general framework to the two levels of application is illustrated in Figure 3. As can be seen, the steps are quite similar to those suggested by the general framework. It is also apparent that the phases of the general framework imply the need for an integrated analysis of dependent and independent events, rather than separate disjoint tasks for each type of event.

Because of the need for an integrated analysis of dependent and independent events; the requirement to support different levels of analysis; and the existence of two different PRA modeling approaches, atotal of five separate procedures can be distinguished within the guide-book as illustrated in Figure 4. This set of procedures includes four for plant-level PRA applications and one for system-level applications. The latter is generally applicable to different modeling techniques, such as fault trees, reliability block diagrams, and GO models. The plant-level procedures are determined by the scope of the PRA and whether an event tree or a fault tree-based technique for modeling intersystem dependencies is used.

## APPLICATIONS

A preliminary version of the system-level procedures of this guidebook has been used in an international benchmark exercise on common cause analysis (Reference 9). The U.S. contribution to this benchmark exercise was jointly sponsored by EPRI and the NRC. The aspects of the procedures that were given particular emphasis in this analysis was the classification of event data, using an extension of the system described in References 7 and 8, and the incorporation of these data into the reliability analysis of a system in the plant at Grohnde Federal Republic

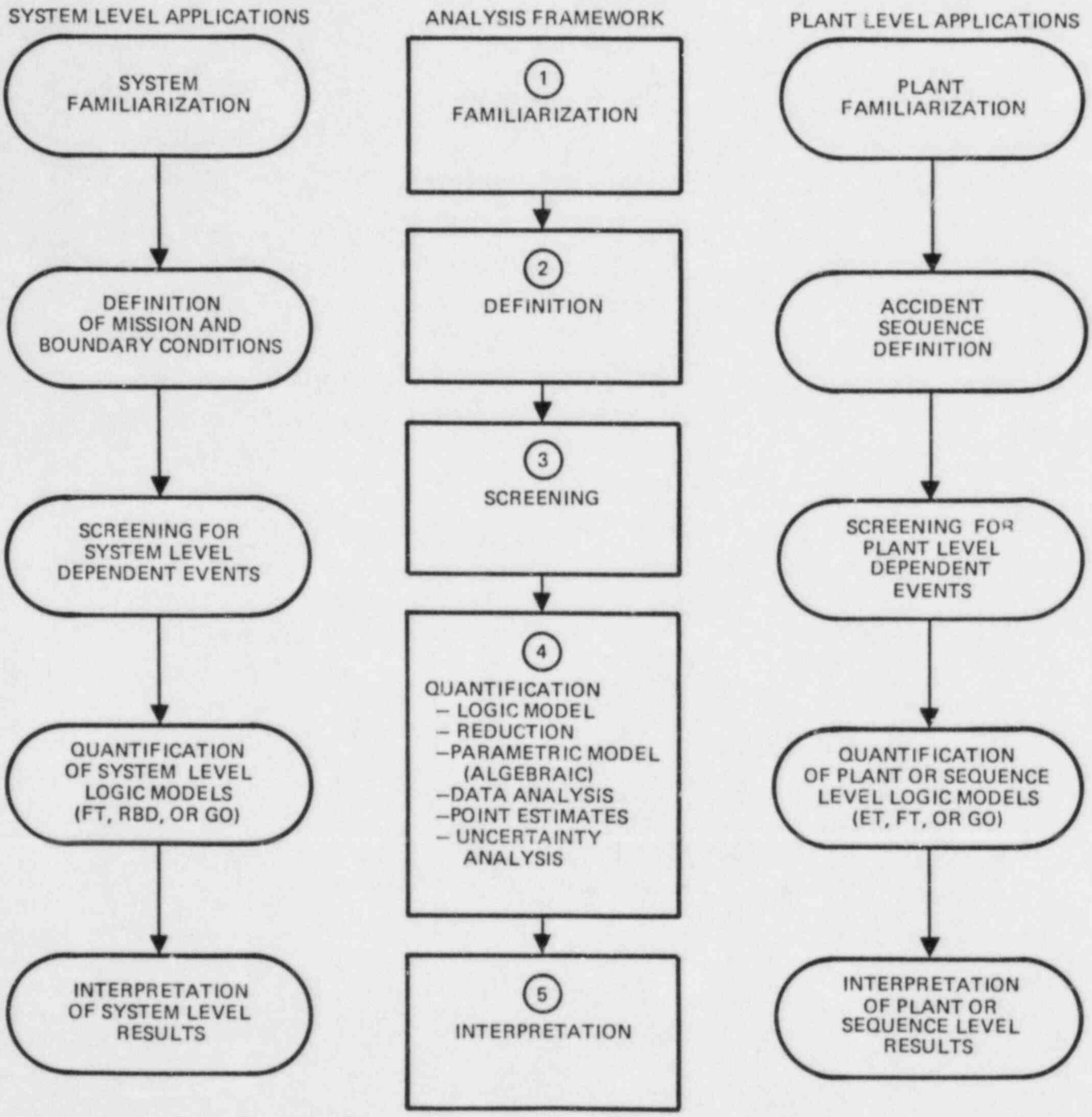


FIGURE 3. COMMON FRAMEWORK FOR PLANT AND SYSTEM LEVEL DEPENDENT EVENTS ANALYSIS

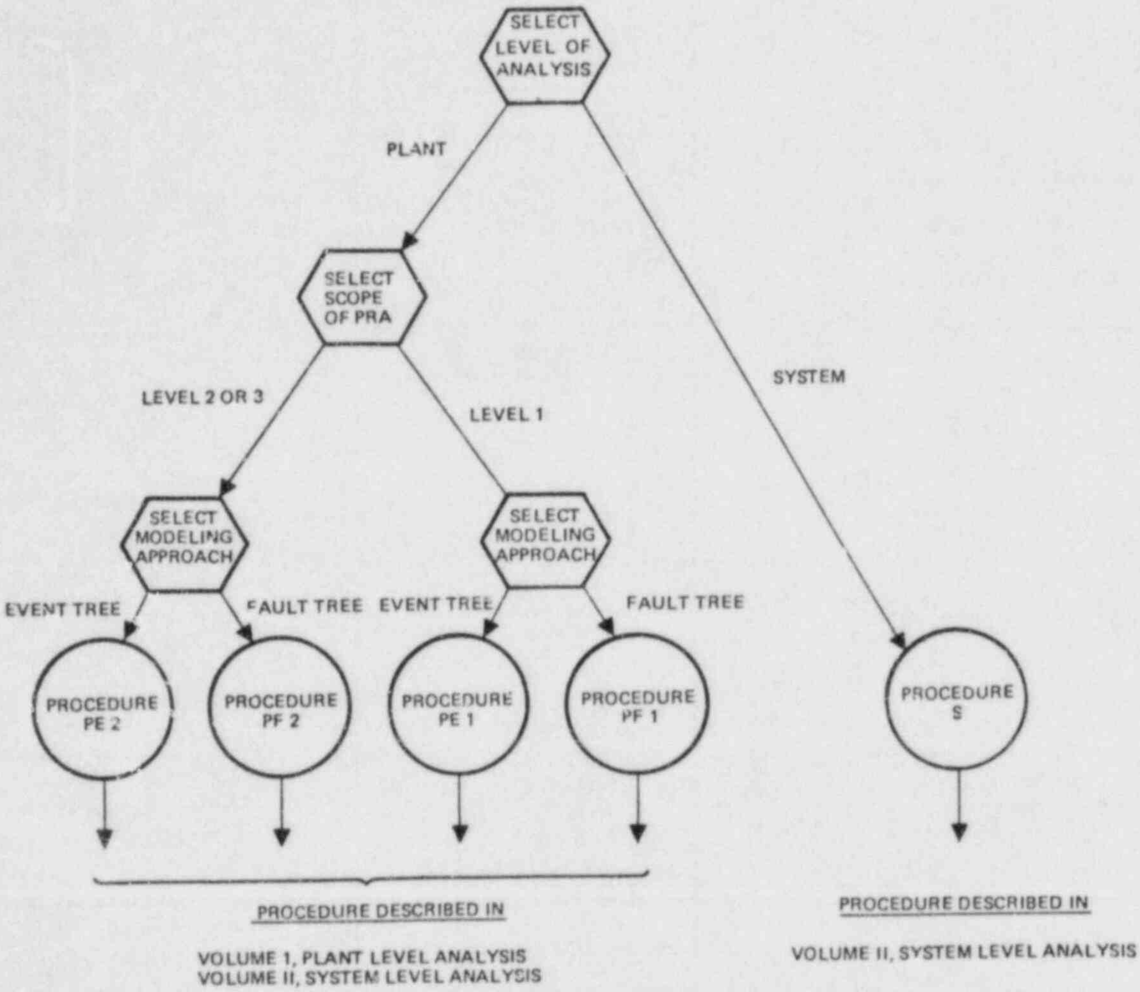


FIGURE 4. DECISION TREE FOR SELECTING DEPENDENT EVENTS ANALYSIS PROCEDURES AND GUIDEBOOK VOLUMES

of Germany built by KWU. One aspect of this analysis was the evaluation of U.S. data on auxiliary feedwater pumps originally developed (Reference 7) for their applicability to the auxiliary feedwater pumps at Grohnde. This applicability is assessed in terms of probability of impact vectors for each event, the analyst's judgment as to how many pumps at Grohnde would have failed when the event is assumed to occur, under the specific design and operational characteristics of that plant. The classification of events involving auxiliary feedwater pumps is illustrated in Table 3. At the bottom of the table is a new frequency distribution of event data created for this plant. The fractional values are the result of classification uncertainty. A worksheet for developing mean estimates of the parmeters of the MGL model (Reference 1) from such data as these for diesel-driven pumps is illustrated in Table 4.

There were nine additional teams from countries of the European Economic Community who analyzed the same problem as did the U.S. team. Some of the key results are presented in Table 5. All the teams are currently in the process of revising their analyses and working toward the development of a consensus approach. The plan is to incorporate the final approach of the benchmark exercise, as well as the parallel contributions from such NRC-funded projects as RMIEP, into the final form of the EPRI-sponsored guidebook on dependent events analysis. As can be seen from the preliminary results of the benchmark exercise in Table 5, certain elements of a consensus approach are already beginning to emerge.

## REFERENCES

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3. Edwards, G. T., and I. A. Watson, "A Study of Common-Mode Failures," UKAEA Systems Reliability Directorate Report SRD-146, July 1979.

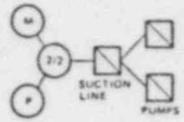
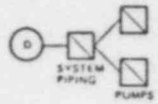
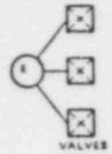
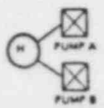
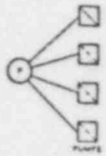
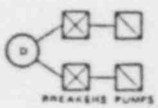
4. Rasmussen, D. M., et al. "COMCAN II-A - A Computer Program for Automated Common-Cause Failure Analysis," TREE-1361, EG&G Idaho, Inc., INEL, 1979.

5. American Nuclear Society and IEEE, "PRA Procedures Guide; A Guide to the Performance of Probabilistic Risk Assessments for Nuclear Power Plants," U.S. Nuclear Regulatory Commission, NUREG/CR-2300, 1983.

6. Fleming, K. N., et al., "Systems Interaction Identification Procedures - Volume 5 Application of PRA to the System Interaction Issue," prepared for the Electric Power Research Institute, EPRI-NP-3834 by Pickard, Lowe and Garrick, Inc., July 1985.

TABLE 3. CLASSIFICATION AND IMPACT ASSESSMENT OF EVENTS INVOLVING DEPENDENT FAILURES AND UNAVAILABILITIES OF AUXILIARY FEEDWATER PUMPS (Reference 9)

Sheet 1 of 2

| Plant (date) | Status | Event Description | Cause-Effect Diagram | Application | $P_0$ | $P_1$ | $P_2$ | $P_3$ | $P_4$ | N/A |
|---|---|---|---|---|---|---|---|---|---|---|
| Ginna (December 1973) | Critical | Two motor-driven auxiliary feedwater pumps inoperable due to air in common suction line. |   | Ginna | 0 | 0 | 1 | 0 | 0 | 0 |
| | | | | Grohnde | 0 | 0 | 0 | 0 | 0 | 1 |
| Zion 2 (February 1974) | Power Escalation Test | Two motor-driven auxiliary feedwater pumps inoperable due to air in suction lines. |   | Zion 2 | 0 | 0 | 1 | 0 | 0 | 0 |
| | | | | Grohnde | 0 | 0 | 0 | 0 | 0 | 1 |
| Kewaunee (November 1975) | Shutdown | Resin clogged auxiliary feedwater pump strainers causing reduced flow. |   | Kewaunee | 0.9 | 0 | 0 | 0.1 | 0 | 0 |
| | | | | Grohnde | 0.9 | 0 | 0 | 0 | 0.1 | 0 |
| Turkey Point 3 (May 1974) (two events) | 98% Power | Auxiliary feedwater pumps A and B failed to start due to tight packing. Pump C started, but tripped due to governor failure. |   | Turkey Point 3 | 0 | 0 | 1 | 0 | 0 | 0 |
| | | | | Grohnde | 0 | 0 | 0 | 0 | 0 | 1 |
| Point Beach 1 and 2 (April 1974) | Power | Preoperation strainers left in suction line plugged, making motor-driven auxiliary feedwater pump A on Unit 1 inoperable. Similar strainers were found in Unit 1 motor-driven auxiliary feedwater pump B and Units 1 and 2 turbine-driven auxiliary feedwater pumps. |   | Point Beach 1 | 0 | 0.9 | 0 | 0 | 0.1 | 0 |
| | | | | Grohnde | 0 | 0.9 | 0 | 0 | 0.1 | 0 |
| Zion 2 (September 1981) | Shutdown | All three auxiliary feedwater pumps failed to start. Pumps 2B and 2C failed due to a backfeed circuit that resulted from pump control switch modification. Failure of pump 2A was due to a pressure switch drift. |   | Zion 2 | 0 | 0 | 1 | 0 | 0 | 0 |
| | | | | Grohnde | 0 | 0 | 0 | 0 | 0 | 1 |



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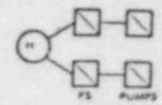
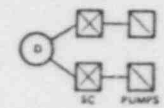
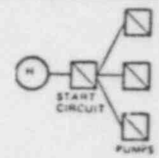
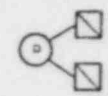
## TABLE 3 (continued)

Sheet 2 of 2

| Plant (date) | Status | Event Description | Cause-Effect Diagram | Application | $P_0$ | $P_1$ | $P_2$ | $P_3$ | $P_4$ | N/A |
|---|---|---|---|---|---|---|---|---|---|---|
| Zion 2 (November 1979) | Power | Auxiliary feedwater pumps 2B and 2C failed to start due to miscalibrated pressure gauges. |   | Zion 2 | 0 | 0 | 1 | 0 | 0 | 0 |
| | | | | Grohnde | 0 | 0 | 0 | 0 | 0 | 1 |
| Zion 2 (December 1979) | Power | Auxiliary feedwater pumps 2B and 2C failed to start due to start circuitry design problem. |   | Zion 2 | 0 | 0 | 1 | 0 | 0 | 0 |
| | | | | Grohnde | 0 | 0 | 0 | 0 | 0 | 1 |
| Turkey Point 4 (June 1973) | Prior to Initial Power Testing | All three auxiliary feedwater pumps failed to start automatically due to missing fuses in pump autostart circuit. |   | Turkey Point 4 | 0 | 0 | 0 | 0 | 0 | 1 |
| | | | | Grohnde | 0 | 0 | 0 | 0 | 0 | 1 |
| Arkansas One 2 (April 1980) | 0% Power | Two emergency feedwater pumps lost suction due to steam flushing; system design problem. |   | ANO 2 | 0 | 0 | 1 | 0 | 0 | 0 |
| | | | | Grohnde | 0 | 0 | 0.9 | 0 | 0.1 | 0 |
| | | | | Total | 0.9 | 0.9 | 0.9 | 0 | 0.3 | 7 |

LEGEND:

Cause-Effect Diagram:

M - Maintenance
P - Procedural Error
D - Design Error
E - Environmental
I - Internal Failure
H - Human Error



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TABLE 4. COMMON CAUSE DATA SUMMARY WORK SHEET FOR
DIESEL-DRIVEN AUXILIRY FEEDWATER PUMPS (REFERENCE 9)

Component: EFW Pump and Diesel Block

Failure Mode: All

Group Size: 4

Impact Vector:

| $n_0$ | $n_1$ | $n_2$ | $n_3$ | $n_4$ | $n_5$ | $n_6$ | N/A* |
|---|---|---|---|---|---|---|---|
| 5.4 | 3.6 | 4.4 | 1.4 | 10.2 | -- | -- | 7 |

Other Independent Events = 628.1

MGL Mean Values:

$$\beta = \frac{2n_2 + 3n_3 + 4n_4 + 1}{n_1 + 2n_2 + 3n_3 + 4n_4 + 2} = \frac{54.8}{687.5} = 0.08$$

$$\gamma = \frac{3n_3 + 4n_4 + 1}{2n_2 + 3n_3 + 4n_4 + 2} = \frac{46.0}{55.8} = 0.82$$

$$\delta = \frac{4n_4 + 1}{3n_3 + 4n_4 + 2} = \frac{41.8}{47.0} = 0.89$$

* N/A = not applicable

TABLE 5. PRELIMINARY RESULTS OF THE CCF-RBE BENCHMARK

| Team | Technical Approach | | Top Event Unavailability | Type of Estimate |
|---|---|---|---|---|
| | Qualitative | Quantitative | | |
| A. Belgium | FMEA | Beta Factor | 6.5(-7) to 1.5(-5) | Range based on point estimates. |
| B. Denmark | System Familiarization | Binomial Failure Rate | 7.0(-5) to 1.1(-4) | Monte Carlo trials uncertainty. |
| C. Federal Republic of Germany-1 (GRS) | System Familiarization | Beta Factor, Marshall-Olkin | 4(-5)<br>1(-4) | Mean of uncertainty distribution.<br>95th percentile of uncertainty distribution. |
| D. Federal Republic of Germany-2 (KFA) | Se[illegible]tivity Analysis | Marshall-Olkin | 4(-6) to 1(-5) | Range based on point estimates. |
| E. Federal Republic of Germany-3 (KWU) | System Familiarization | Multiple Greek Letter | 4.6(-6) | Point estimate. |
| F. France | Generic Cause Analysis | Multiple Greek Letter | 3.6(-7) to 1.5(-6) | Range based on point estimates. |
| G. Italy | System Familiarization | Binomial Failure Rate | 1.2(-4) | Point estimate. |
| H. Sweden | System Familiarization | Multiple Greek Letter | 3.0(-7) to 4.5(-5) | Range based on point estimates. |
| I. United Kingdom | FMEA/Checklist | Modified Beta Factor, Cutoff | 1.6(-5) to 5.0(-4) | Range based on point estimates. |
| J. United States | System Familiarization | Multiple Greek Letter | 3.6(-5)<br>9.5(-5) | Point estimate, U.S. component data.*<br>Point estimate, KWU component data.* |

*All teams performed at least one calculation using KWU Component Data for independent events, U.S. common cause event data was used in both estimates to estimate MGL parameters.

7. Smith, A. M., et al., "A Study of Common Cause Failure--Phase II: A Comprehensive Classification System for Component Fault Analysis," prepared for the Electric Power Research Institute, by Los Alamos Technical Associates," EPRI NP-3837, May 1985.

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9. Fleming, K. N., et al., "United States Contribution to Common Cause Failure Reliability Benchmark Exercise," Pickard, Lowe and Garrick, Inc., PLG-0426, July 1985.

# THE MECHANICS OF INTEGRATING ROOT CAUSES INTO PRAs

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EG&G Idaho, Inc.

## ABSTRACT

This paper presents a derivation of root cause importance, root cause data for selected components of a pressurized water reactor auxiliary feedwater system, an Accident Sequence Evaluation Program (ASEP) auxiliary feedwater system model, and the results of root cause importance calculations. The methodology shown herein is straightforward and is easily applied to existing probabilistic risk assessments. Root cause importance can greatly benefit the areas of design, maintenance, and inspection. Root cause importance for various components and circumstances can be evaluated.

## ACKNOWLEDGMENTS

The authors wish to express their thanks to Nathan G. Cathey for his support with the ASEP related importance example, and also for his guidance and review of this paper. Thanks also go to Corwin L. Atwood, who was instrumental in developing the form of the root cause importance equation given in this paper.

## NOMENCLATURE

### Acronyms

| | |
|---|---|
| AFW | Auxiliary Feedwater System |
| ASEP | Accident Sequence Evaluation Program |
| CST1, 2 | Condensate Storage Tank 1, or 2 |
| FO | Fails to Open failure mode |
| FP | Fails to Operate (Run) failure mode |
| FS | Fails to Start failure mode |
| LER | Licensee Event Report data base |
| MOV | Motor Operated Valve |
| NPRDS | Nuclear Plant Reliability Data System data base |
| SC | Spurious Closure failure mode |

a. Presently with Science Applications International Corporation (SAIC).

TDP Turbine Driven Pump

<u>Variables</u>

| | |
|---|---|
| A | The failure mode weighting factor found from Accident Sequence Evaluation Program source data |
| B | The product of (A)(r) |
| B | An expression for 1 - (A)(r) |
| $I^{FV}$ | The Fussell-Vesely importance function |
| $I_i$ | The root cause importance function |
| n | A number of root caused failures |
| Q | Component unavailability or system unavailability |
| Q* | Cut set unavailability |
| r | Root cause fraction |
| t | Time; in this case, it is the mission time |
| $\lambda$ | Failure rate (per hour) |
| $\lambda_D$ | Demand failure rate |

<u>Superscripts</u>

| | |
|---|---|
| i | Denotes a particular root cause or a root cause related value |

<u>Subscripts</u>

| | |
|---|---|
| i | Denotes a particular root cause or a root cause related value |

## INTRODUCTION

The Root Causes of Component Failures Program has been initiated by the U.S. Nuclear Regulatory Commission (NRC) for the purpose of identifying root causes of failures for components that comprise safety and safety support systems in nuclear power plants. Root causes are defined as underlying or initiating events or conditions that produce a component failure, either alone or in combination with preexisting or immediate conditions that could not produce a failure. The identification, collection, and evaluation of root causes will provide information on why components fail. With the development of application methods, root cause data can provide additional insights in the areas of (a) probabilistic risk assessments, (b) reliability assurance, and (c) application of risk assessments to inspection programs.

Under the root cause program, begun in 1984, a root cause categorization scheme has been compiled. This root cause categorization scheme has been tested by applying it to failures described in existing failure reporting data bases [Licensee Event Reports (LER), Nuclear Plant Reliability Data System (NPRDS), etc.].[1] Based on the positive results from the trial application, root causes for several major components have been identified and collected. In addition, the methodology for integrating root cause data into existing probabilistic risk assessments (PRAs) has been developed.

An example of the use of root cause data is illustrated here by performing a derivation of root cause fractions and a calculation of root cause importances. An auxiliary feedwater (AFW) system model from the Accident Sequence Evaluation Program (ASEP) was used for the evaluation. The root causes for failures of the turbine driven AFW pumps and motor operated valves (MOVs) are specifically examined for their importance to auxiliary feedwater unavailability. The ASEP basic event unavailabilities are decomposed by failure mode to allow for root cause application to specific failure modes. The root cause importance is presented here for specific failure modes.

## METHODOLOGY FOR INTEGRATING ROOT CAUSES INTO EXISTING PRAs

Root causes of component failures can be collected and grouped by component and component failure mode. With this information, root cause fractions are calculated. These give the fraction of failures that are attributed to a particular root cause. The root cause fractions can be applied to determine relative contributions of the root causes to system unavailability, accident sequence frequence, core melt probability, and risk. The evaluations involved in extending PRAs to include root cause information are presented in this section.

### Root Cause Fractions

The calculation of root cause importances is carried out by using a root cause fraction as a multiplier on the component failure rate. A root cause fraction for cause i is defined[2] as

$$r_i = \frac{n_i}{n} \qquad (1)$$

where

$n_i$ = the number of failures which are due to root cause i

$n$ = the total number of failures for the component of interest

The root cause fraction is simply the fraction of all failures due to a particular root cause. Given the definition of $r_i$, it can be seen that the r values will sum to 1,[3]

$$\sum_{i}^{k} r_i = 1 \qquad (2)$$

where

$k$ = the total number of root causes identified for the component of interest.

The root cause fraction definition can be straightforwardly extended to consider specific failure modes

$$r_i = \frac{n_i}{n} \tag{3}$$

where

$r_i$ = the failure mode specific root cause fraction for root cause i and failure mode (fm).

$n_i$ = the number of failures which are due to root cause i and result in failure mode (fm).

$n$ = the total number of component failures which result in failure mode (fm).

The component failure rate, $\lambda$, is found by dividing the number of component failures by the product of the component population and the time span of interest. When the $r_i$ values are known, the failure rate can be weighted by using $r_i$ as a multiplier,

$$\lambda_i = \lambda \, r_i \tag{4}$$

where

$\lambda$ = the component failure rate (per hour)

$\lambda_i$ = the failure mode specific, root cause i failure rate.

With this development, component unavailabilities can be decomposed into root cause contributions. Consider the hardware unavailability, Q, of a component, which can generally be expressed as[4]

$$Q = \lambda \, t \tag{5}$$

where

$t$ = a time of interest.

The use of the root cause fraction on the failure rate gives a root cause unavailability for a specific failure mode, $Q_i$, as

$$Q_i = \lambda_i \, t = r_i \, \lambda t = r_i \, Q \; . \tag{6}$$

Equation (6) can also be written for demand failures

$$Q_i = \lambda_D \, r_i \tag{7}$$

where

$\lambda_D$ = the demand failure rate (per cycle).

These root cause unavailabilities can be incorporated into a cut set unavailability calculation. All weightable basic event Q values are treated with root cause fractions and root cause cut set unavailabilities are obtained. By the use of the r values as multipliers, fault trees and minimal cut sets can be used for further analysis. This means existing PRA results can be modified to evaluate the effect of root causes without regenerating the cut sets.

## Root Cause Importance

With root cause fractions it is possible to determine the effect of a particular root cause on the fault tree top event occurrence. One method of performing this is by using the Fussell-Vesely importance function, $I^{FV}$. This function is defined as[5]

$$I^{FV} = \left(\sum^{m} Q^*\right) / Q \tag{8}$$

where

$m$ = the number of cut sets that contain the basic event of interest

$Q$ = the system total or top event unavailability

$Q^*$ = cut set unavailability.

Equation (8) can be modified to yield root cause importance. The numerator is redefined to be a summation of root cause weighted $Q^*$ values. Let

$$B^i = A \, r_i \tag{9}$$

where

$A$ = the ASEP hardware weighting factor. For example, the TDP FS value is (pump failures in the FS mode)/(total pump failures).

Then define the complement, $\bar{B}^i$, as

$$\bar{B}^i = 1 - A \, r_i \quad . \tag{10}$$

Using Equation (10), an expression for the summation of all necessary root cause weighted values is obtained.

$$\text{Num} = \sum^{n} Q^{*} \left[1 - \left(\overset{j}{\pi} \; \bar{B}^{i}_{j}\right)\right] \tag{11}$$

where

$Q^{*}$ = a cut set unavailability

$n$ = the number of cut sets that contain component basic events of interest

$j$ = the number of basic events in a cut set of interest

The bracketed term in Equation (11) is the probability that at least one of the cut set components is failed due to root cause i. Equation (11) is the root cause contribution to the system unavailability. The normalized importance of the root cause i over all root causes is

$$I_i = \text{Num}/\sum_{\text{all}} \text{Num} = \sum^{n} Q^{*} \left[1 - \left(\overset{j}{\pi} \; \bar{B}^{i}_{j}\right)\right] / \sum_{\text{all}} \text{Num} \quad . \tag{12}$$

Each root cause cut set unavailability, $Q^{*}\,(1 - \pi\bar{B}^{i})$, is simply the unavailability which is caused by root cause i. The root cause importance, $I_i$, is the fraction of system unavailability due to a particular root cause.

## EXAMPLE APPLICATION OF THE METHODOLOGY TO AN AFW SYSTEM

A simple calculation of root cause importance is illustrated here using an AFW model[6] from the ASEP. The root causes of failures for the turbine driven AFW pumps and the MOVs will be specially examined in this example to demonstrate the mechanics of applying the methodology to integrate root causes into existing PRAs.

### Description of the System Model

The particular AFW model chosen utilizes two TDPs. The system schematic is shown in Figure 1. Only the two dominant pump failure modes, failure to operate (FP) and failure to start (FS), and the two dominant valve failure modes, failure to open (FO) and spurious closure (SC), were examined for their root cause importance. Failures of other components in the AFW system are not decomposed into root cause contributions.

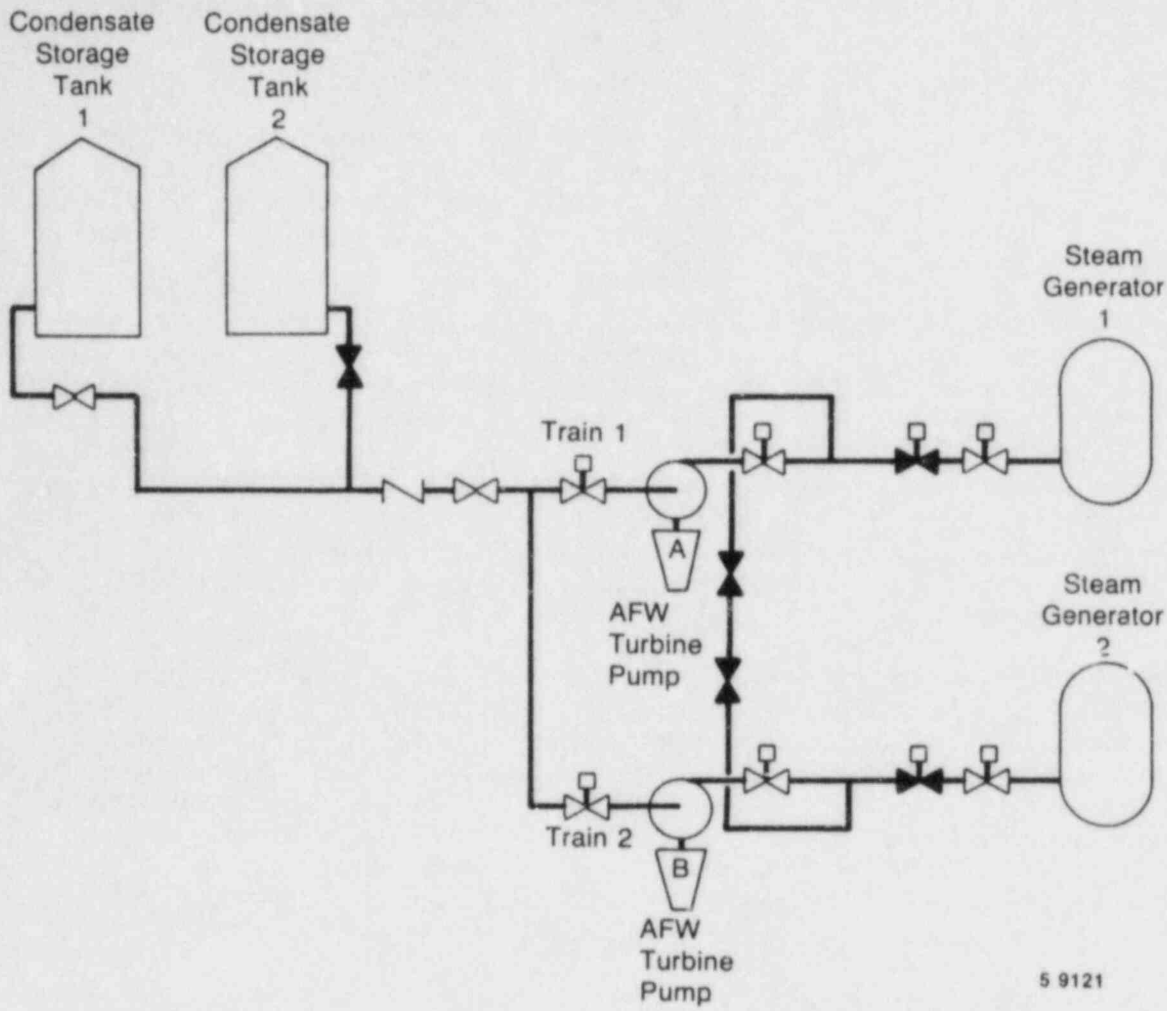


Figure 1. Auxiliary feedwater system Model 8, schematic diagram.

The pump and valve unavailabilities are decomposed into failure mode contributions to align the unavailabilities with failure mode specific root cause fractions. This allowed the determination of root cause contributions to specific failure modes. The cut sets, ASEP conditional unavailabilities, and hardware weighting factors are shown in Appendix A.

There are several assumptions made for this example:

1. The pump units were assumed to be the bulk of the AFW hardware basic events. This resulted in slightly conservative pump failure probabilities.

2. Only hardware failures were examined. Actuation failures, test and maintenance outages, electric power faults, and recovery actions were not considered.

3. The accident sequence probabilities from ASEP were converted into conditional unavailabilities by division of the initiating event frequency for transients that leave the power conversion system

available. Cut sets with sequence probabilities lower than $10^{-8}$ were not considered in this analysis, so the unavailabilities are conditional numbers.

4. The SC failure mode for MOVs has not yet been specifically examined by the root cause program. Therefore, root cause counts were contrived for this failure mode from the valve data summary.[7] The other root cause data was taken from the LER and NPRDS data bases.

5. The root cause fractions do not include unclassifiable entries. Only valid level III root causes (the most detailed root causes) are evaluated here. Techniques are under development to include unclassifiable entries in this type of analysis.

The root cause fractions for TDPs and MOVs from the root cause program are given in Table 1. The ASEP hardware weighting values, cut set conditional unavailabilities, and root cause fractions are needed to calculate root cause importance. The results for this example are given in the next section.

Table 2 shows the normalized root cause importances that were calculated. The following observations will dwell on the three component failure modes for which actual root cause data has been collected. Examination of Table 2 gives Accidental Maintenance Action, Foreign Materials Intrusion, and Initial Design Error as the largest root cause contributions for TDPs failing to start. Similarly, Accidental Maintenance Action, Binding/Out of Adjustment, and Improper Lubrication are the largest root cause contributions for TDPs failing to run. This is more easily seen from the bar charts in Figures 2 and 3.

For MOVs, Binding/Out of Adjustment, Corrosion and Mechanical Set Point Drift gave the largest root cause contributions to the fail to open mode. Bar charts for MOVs are given in Figures 4 and 5.

There are many important utilizations of this kind of root cause information. Reliability programs, technical specifications programs, inspection programs and aging programs can benefit from this information. The next phase of this work will address the uses and interpretations of ro cause information for specific applications.

## CONCLUSIONS

It is seen in this paper that the method of applying root cause information to PRA results is straightforward. Simplicity and the fact that the method builds on existing PRA results gives a cost effective, advantageous means to evaluate root cause impacts. When root cause impacts have been identified, work on corrective measures to eliminate or reduce significant root cause impacts can be performed. As more root cause coding is performed, the root cause fractions will become better defined. More components, and more of their failure modes, will be considered, leading to a wider application of the methodology.

TABLE 1. ROOT CAUSE FRACTIONS FOR TURBINE DRIVEN PUMPS AND MOTOR OPERATED VALVES

| | | Root Cause Fractions For Each Component Failure Mode | | | |
|---|---|---|---|---|---|
| Root Cause | Root Cause Code | Turbine Driven Pump, Fails to Start | Turbine Driven Pump, Fails to Run | Motor Operated Valve, Fails to Open | Motor Operated Valve, Spurious Closure |
| Initial Design Error | DEI | 0.115 | 0.051 | 0.061 | 0.037 |
| Manufacturing Error | DM | -- | -- | 0.030 | 0.037 |
| Inadequate Maintenance Procedures | SPM | -- | 0.026 | -- | -- |
| Inadequate Operations Procedures | SPO | 0.051 | -- | -- | -- |
| Inadequate Testing Procedures | SPT | 0.039 | 0.051 | -- | -- |
| Inadequate Training in Calibration | STC | -- | -- | 0.091 | -- |
| Inadequate Training in Operations | STO | -- | -- | 0.061 | -- |
| Inadequate Training in Testing | STT | -- | 0.026 | -- | -- |
| Contractor Error | SC | 0.038 | 0.026 | -- | -- |
| Accidental Maintenance Action | HAM | 0.192 | 0.128 | -- | -- |
| Accidental Operations Action | HAO | 0.039 | 0.026 | -- | -- |
| Accidental Testing Action | HAT | -- | 0.026 | -- | 0.037 |
| Failure to Follow Maintenance Procedures | HPM | -- | 0.051 | -- | -- |
| Failure to Follow Operations Procedures | HPO | 0.038 | -- | -- | -- |
| Failure to Follow Testing Procedures | HPT | 0.039 | 0.051 | 0.030 | -- |
| Adverse Atmospheric Condition | ENA | -- | 0.026 | -- | -- |
| Corrosion | ECC | 0.038 | 0.026 | 0.121 | 0.148 |
| Arcing | ELA | 0.039 | -- | -- | -- |
| Electrical Set Point Drift | ELD | 0.038 | 0.051 | -- | -- |
| Insulation Breakdown | ELI | -- | -- | 0.030 | -- |
| Wear | EBR | -- | 0.077 | 0.030 | -- |
| Binding/Out of Adjustment | EDB | 0.077 | 0.128 | 0.273 | 0.370 |
| Foreign Materials Intrusion | EDI | 0.154 | 0.026 | 0.030 | 0.296 |
| Improper Lubrication | EDU | 0.039 | 0.102 | 0.061 | -- |
| Mechanical Overload | EDO | 0.038 | -- | -- | -- |
| Mechanical Set Point Drift | EDS | 0.039 | 0.026 | 0.121 | -- |
| Water Intrusion | EMW | -- | -- | 0.030 | -- |
| Flow Induced Vibration | EVF | 0.038 | -- | -- | -- |
| Mechanical Vibration | EVM | -- | 0.026 | 0.030 | 0.074 |

TABLE 2. NORMALIZED ROOT CAUSE IMPORTANCE FOR TDPs AND MOVs

| Root Cause | Root Cause Code | Normalized Root Cause Importance | | | |
|---|---|---|---|---|---|
| | | Turbine Driven Pump, Fails to Start | Turbine Driven Pump, Fails to Run | Motor Operated Valve, Fails to Open | Motor Operated Valve, Spurious Closure |
| Initial Design Error | DEI | 9.81E-02 | 2.46E-03 | 3.75E-03 | 7.13E-05 |
| Manufacturing Error | DM | 0 | 0 | 1.89E-03 | 7.13E-05 |
| Inadequate Maintenance Procedures | SPM | 0 | 1.23E-03 | 0 | 0 |
| Inadequate Operations Procedures | SPO | 0 | 2.46E-03 | 0 | 0 |
| Inadequate Testing Procedures | SPT | 3.50E-02 | 2.46E-03 | 0 | 0 |
| Inadequate Training in Calibration | STC | 0 | 0 | 5.65E-03 | 0 |
| Inadequate Training in Operations | STO | 0 | 0 | 3.50E-03 | 0 |
| Inadequate Training in Testing | STT | 0 | 1.23E-03 | 0 | 0 |
| Contractor Error | SC | 3.50E-02 | 1.23E-03 | 0 | 0 |
| Accidental Maintenance Action | HAM | 1.65E-01 | 6.15E-3 | 0 | 0 |
| Accidental Operations Action | HAO | 3.50E-02 | 1.23E-03 | 0 | 0 |
| Accidental Testing Action | HAT | 0 | 1.23E-03 | 0 | 7.13E-05 |
| Failure to Follow Maintenance Procedures | HPM | 0 | 2.46E-03 | 0 | 0 |
| Failure to Follow Operations Procedures | HPO | 3.50E-02 | 0 | 0 | 0 |
| Failure to Follow Testing Procedures | HPT | 3.50E-02 | 2.46E-03 | 1.89E-03 | 0 |
| Adverse Atmospheric Condition | ENA | 0 | 1.23E-03 | 0 | 0 |
| Corrosion | ECC | 3.50E-02 | 1.23E-03 | 7.54E-03 | 2.87E-04 |
| Arcing | ELA | 3.50E-02 | 0 | 0 | 0 |
| Insulation Breakdown | ELI | 0 | 0 | 1.89E-03 | 0 |
| Electrical Set Point Drift | ELD | 3.50E-02 | 2.46E-03 | 0 | 0 |
| Wear | EBR | 0 | 3.69E-03 | 1.89E-03 | 0 |
| Binding/Out of Adjustment | EDB | 6.88E-02 | 6.15E-03 | 1.67E-02 | 7.16E-04 |
| Foreign Materials Intrusion | EDI | 1.34E-01 | 1.23E-03 | 1.88E-03 | 5.77E-04 |
| Improper Lubrication | EDU | 3.50E-02 | 4.93E-03 | 3.75E-03 | 0 |
| Mechanical Overload | EDO | 3.50E-02 | 0 | 0 | 0 |
| Mechanical Set Point Drift | EDS | 3.50E-02 | 1.23E-03 | 7.51E-03 | 0 |
| Water Intrusion | EMW | 0 | 0 | 1.89E-03 | 0 |
| Flow Induced Vibration | EVF | 3.50E-02 | 0 | 0 | 1.44E-04 |
| Mechanical Vibration | EVM | 0 | 1.23E-03 | 1.89E-03 | 0 |

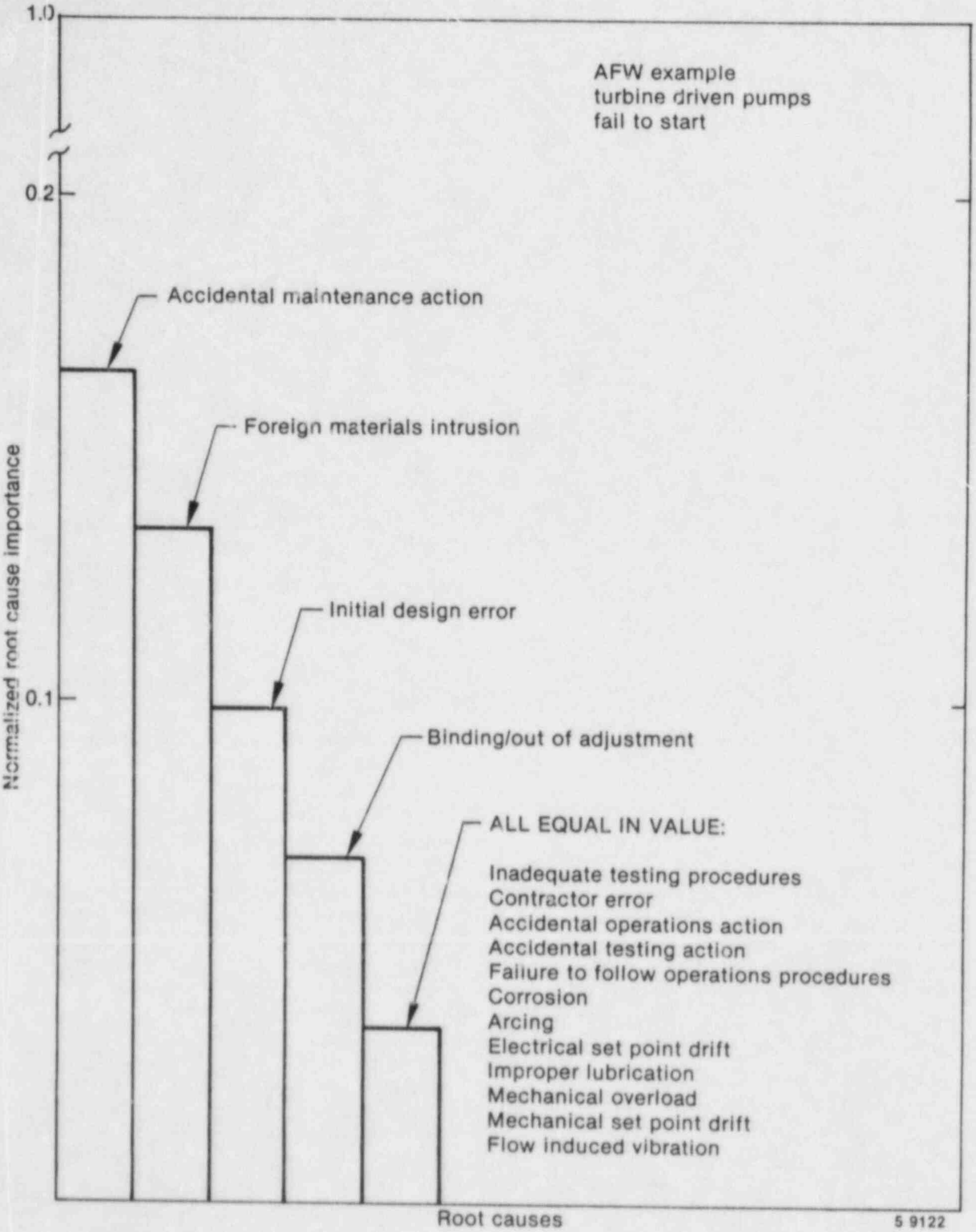


Figure 2. AFW example of turbine driven pumps, failed to start.

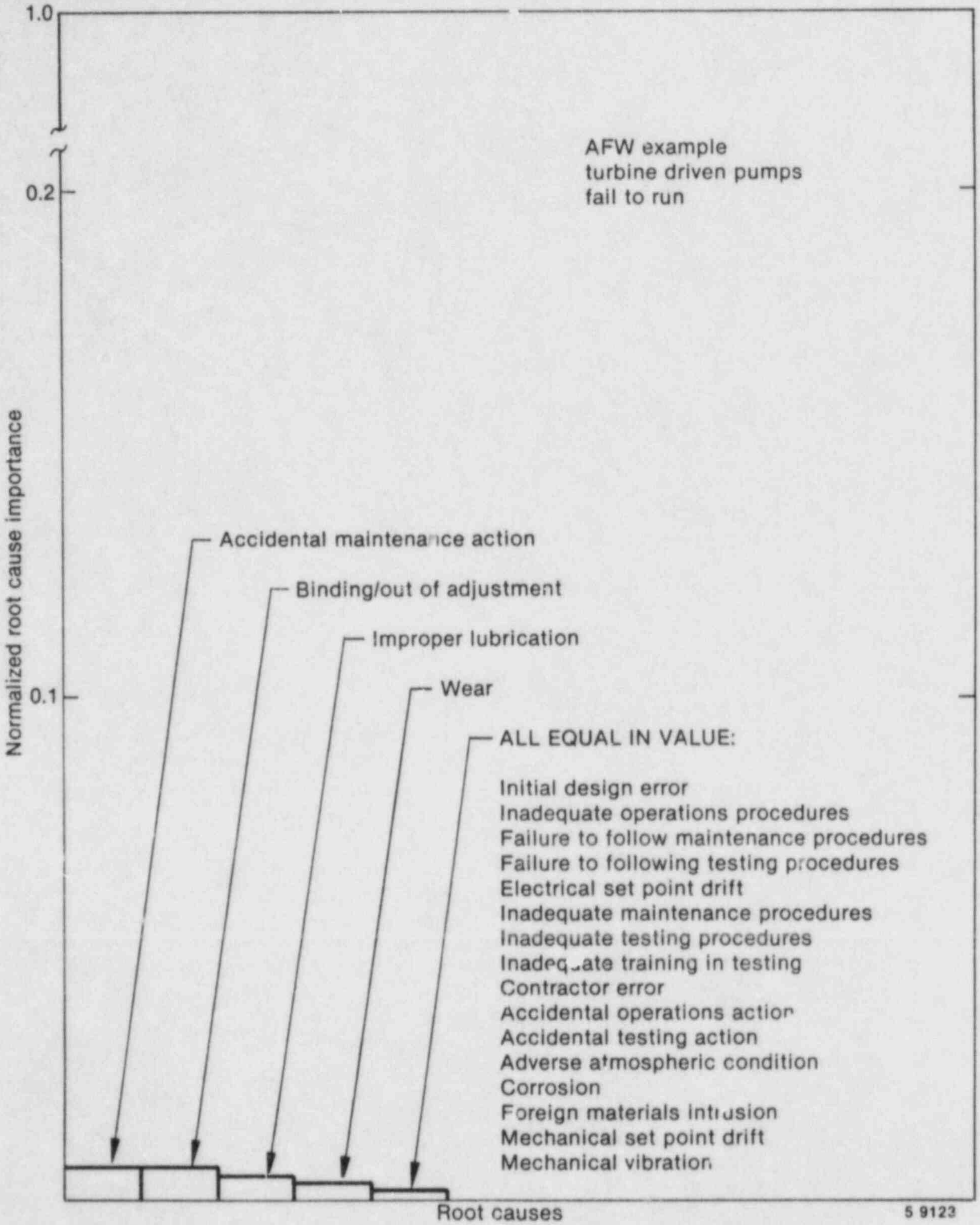


Figure 3. AFW example of turbine driven pumps, failed to run.

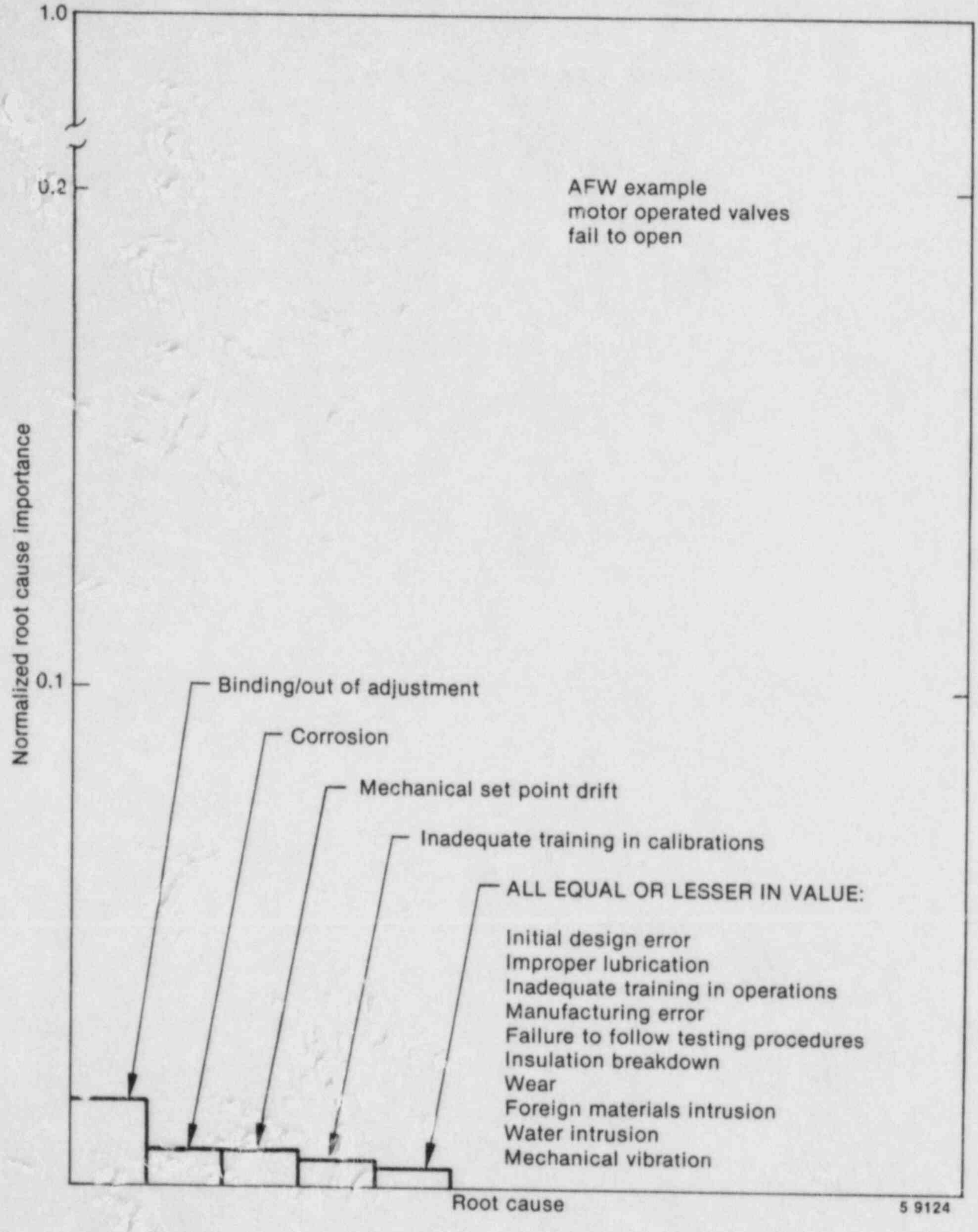


Figure 4. AFW example of motor operated valves, failed to open.

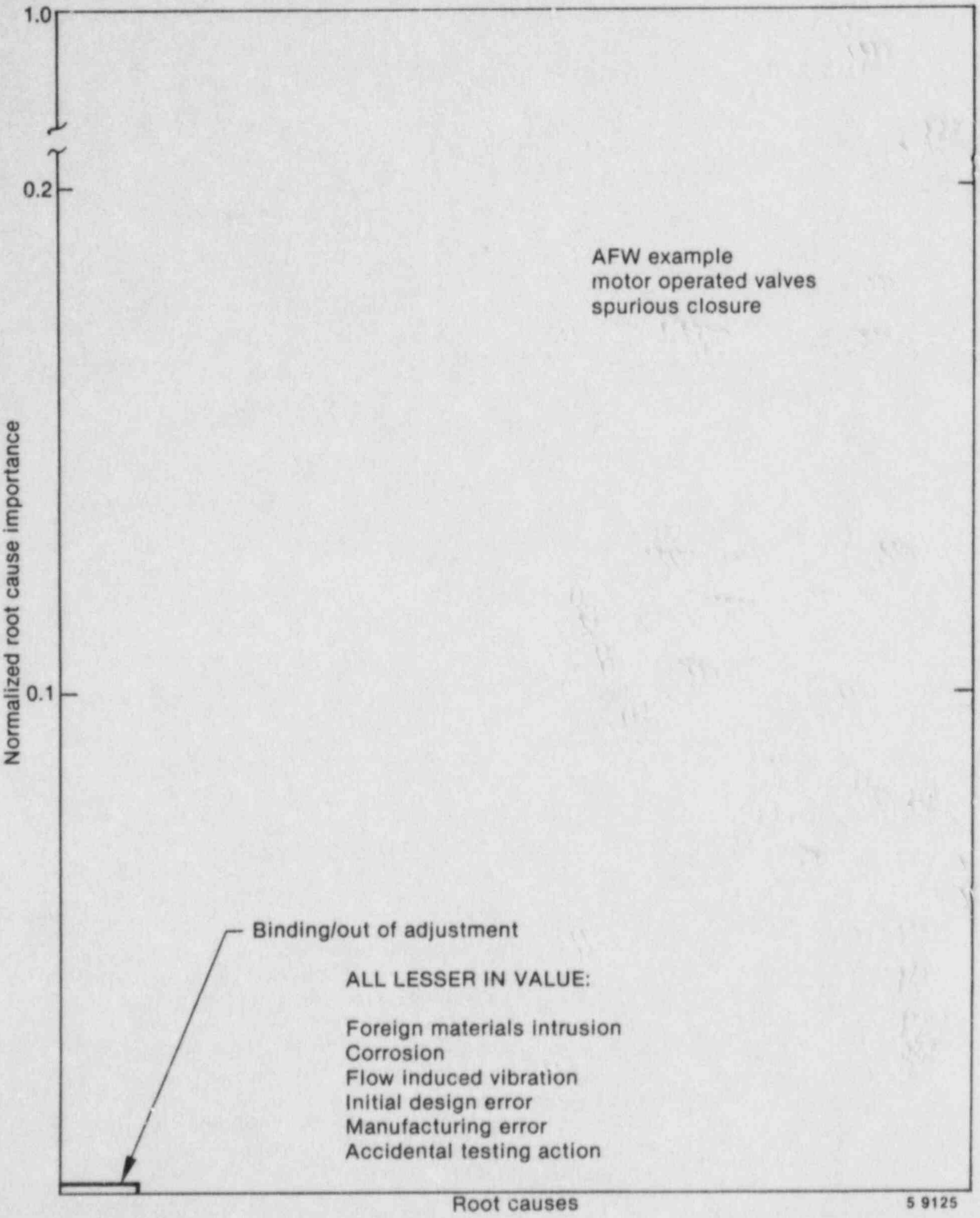


Figure 5. AFW example of motor operated valves, spurious closure.

Other types of importance can be investigated by the use of root cause fractions.[8] For example, the importance of a particular root cause category in causing a system failure can be calculated, or, rather than a cause category, the importance of a given single root cause can be found for the same case. The importance of a root cause in initiating an event requiring safety system response can be determined, or the importance of a root cause in causing safety system degradation can be obtained. The next phase of this program will address various ways root cause information can be utilized for specific applications.

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APPENDIX A

This appendix contains the assumptions used by ASEP to generate the cut sets for the AFW system, a list of those cut sets, hardware weighting factors, and the root cause counts for TDPs and MOVs from the root cause program.

The assumptions used to construct the ASEP AFW system model are listed below:

1. Water is required for one out of the two steam generators for system success.

2. Differences in the configuration of the steam side are not considered to be significant.

3. The system is actuated automatically. Any manual initiation is considered an operator recovery action.

4. This system is totally independent of ac power.

5. No credit is given for the normally closed manual valves in the train 1 to train 2 cross-connect line.

6. It is assumed that no tests or maintenance will be performed on manual valves.

7. The water supply for this system comes from condensate storage tank 1 (CST1). Condensate storage tank 2 (CST2) is assumed to be for recovery purposes only.

Table A-1 gives the ASEP cut sets and their unavailabilities. Table A-2 shows the weighted basic events and the A values. Table A-3 gives the root cause counts that were used to produce the root cause fractions.

TABLE A-1. ASEP CUT SETS AND UNAVAILABILITIES

| Cut Set Number | Cut Set | ASEP Conditional Unavailability, Q* |
|---|---|---|
| 1 | M2 * Recovery * Train 1 HW * Train 2 HW | 1.00E-04 |
| 2 | M2 * Recovery * Train 1 HW * Train 2 T&M | 4.95E-05 |
| 3 | M2 * Recovery * Train 2 HW * Train 1 T&M | 4.95E-05 |
| 4 | M2 * Recovery * Train 1 and 2 CE | 1.18E-05 |
| 5 | M2 * Train 2 HW * Recovery * Train 1 INJ | 1.55E-06 |
| 6 | M2 * Train 1 HW * Recovery * Train 2 INJ | 1.55E-06 |
| 7 | M2 * Recovery * Train 1 INJ * Train 2 INJ | 1.35E-06 |
| 8 | M2 * Train 1 T&M * Recovery * Train 2 INJ | 7.69E-07 |
| 9 | M2 * Train 2 T&M * Recovery * Train 1 INJ | 7.69E-07 |
| 10 | M2 * Train 2 HW * Recovery * Train 1 INJ T&M | 6.66E-07 |

TABLE A-1. (continued)

| | | |
|---|---|---|
| 11 | M2 * Train 1 HW * Recovery * Train 2 INJ T&M | 6.66E-07 |
| 12 | M2 * Train 2 HW * Recovery * Train 1 ACT | 6.66E-07 |
| 13 | M2 * Train 1 HW * Recovery * Train 2 ACT | 6.66E-07 |
| 14 | M2 * Recovery * Train 1 INJ * Train 2 INJ T&M | 5.80E-07 |
| 15 | M2 * Recovery * Train 2 INJ * Train 1 INJ T&M | 5.80E-07 |
| 16 | Train 1 HW * Recovery * Electric Power Fault | 8.18E-08 |
| 17 | Train 2 HW * Recovery * Electric Power Fault | 8.18E-08 |
| 18 | M2 * Train 2 INJ * Recovery * Train 1 ACT | 7.72E-08 |
| 19 | M2 * Train 1 INJ * Recovery * Train 2 ACT | 7.72E-08 |
| 20 | Train 2 INJ * Recovery * Electric Power Fault | 9.50E-09 |
| 21 | Train 1 INJ * Recovery * Electric Power Fault | 9.50E-09 |

LEGEND

M2 Loss of Power Conversion System

Recovery An operator recovery action fails or the system recovery action fails

Train 1 HW Train 1 pump hardware failure

T&M Train 1 outage for testing and/or maintenance

CE Common element to both trains fails

INJ Train 1 injection valve hardware failure

INJ T&M Train 1 injection valve outage for testing and/or maintenance

ACT Train 1 actuation signal fault

NOTE: The entire sequence conditional unavailability (for 31 cut sets) is $3.42 \times 10^{-4}$.

TABLE A-2. WEIGHTED ASEP BASIC EVENTS AND HARDWARE WEIGHTING FACTORS

| | Hardware Weighting Factors, A | | | |
|---|---|---|---|---|
| Basic Event | Fails to Start | Fails to Operate | Fails to Open | Spurious Closure |
| Train 1 HW | 0.95 | 0.05 | -- | -- |
| Train 2 HW | 0.95 | 0.05 | -- | -- |
| Train 1 and 2 CE | -- | -- | 0.97 | 0.03 |
| Train 1 INJ | -- | -- | 0.97 | 0.03 |
| Train 2 INJ | -- | -- | 0.97 | 0.03 |

TABLE A-3. ROOT CAUSE TALLIES FOR TURBINE DRIVEN PUMPS AND MOTOR OPERATED VALVES

| Root Cause | Root Cause Code | Root Cause Counts For Each Component Failure Mode: Turbine Driven Pump, Fails to Start | Turbine Driven Pump, Fails to Run | Motor Operated Valve, Fails to Open | Motor Operated Valve, Spurious Closure |
|---|---|---|---|---|---|
| Initial Design Error | DEI | 3 | 2 | 2 | 1 |
| Manufacturing Error | DM | 0 | 0 | 1 | 1 |
| Inadequate Maintenance Procedures | SPM | 0 | 1 | 0 | 0 |
| Inadequate Operations Procedures | SPO | 0 | 2 | 0 | 0 |
| Inadequate Testing Procedures | SPT | 1 | 2 | 0 | 0 |
| Inadequate Training in Calibration | STC | 0 | 0 | 3 | 0 |
| Inadequate Training in Operations | STO | 0 | 0 | 2 | 0 |
| Inadequate Training in Testing | STT | 0 | 1 | 0 | 0 |
| Contractor Error | SC | 1 | 1 | 0 | 0 |
| Accidental Maintenance Action | HAM | 5 | 5 | 0 | 0 |
| Accidental Operations Action | HAO | 1 | 1 | 0 | 0 |
| Accidental Testing Action | HAT | 0 | 1 | 0 | 1 |
| Failure to Follow Maintenance Procedures | HPM | 0 | 2 | 0 | 0 |
| Failure to Follow Operations Procedures | HPO | 1 | 0 | 0 | 0 |
| Failure to Follow Testing Procedures | HPT | 1 | 2 | 1 | 0 |
| Adverse Atmospheric Condition | ENA | 0 | 1 | 0 | 0 |
| Corrosion | ECC | 1 | 1 | 4 | 4 |
| Arcing | ELA | 1 | 0 | 0 | 0 |
| Electrical Set Point Drift | ELD | 1 | 2 | 0 | 0 |
| Insulation Breakdown | ELI | 0 | 0 | 1 | 0 |
| Wear | EBR | 0 | 3 | 1 | 0 |
| Binding/Out of Adjustment | EDB | 2 | 5 | 9 | 10 |
| Foreign Materials Intrusion | EDI | 4 | 1 | 1 | 8 |
| Improper Lubrication | EDU | 1 | 4 | 2 | 0 |
| Mechanical Overload | EDO | 1 | 0 | 0 | 0 |
| Mechanical Set Point Drift | EDS | 1 | 1 | 4 | 0 |
| Water Intrusion | EMW | 0 | 0 | 1 | 0 |
| Flow Induced Vibration | EVF | 1 | 0 | 0 | 2 |
| Mechanical Vibration | EVM | 0 | 1 | 1 | 0 |
| Totals | | 26 | 39 | 33 | 27 |

ORGANIZING DEPENDENT EVENT DATA - A CLASSIFICATION AND ANALYSIS OF MULTIPLE COMPONENT FAULT REPORTS

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A Classification System has been developed to provide a logical method for the dissection and reconstruction of malfunction scenarios, and to identify the constituent elements of a scenario in a concise and unambigious terminology. The Classification System is described in some detail, and its application to the analysis of operational malfunction events in nuclear plant Auxiliary Feedwater Systems is illustrated. It is suggested that the Classification System approach and terminology be adopted and used to interpret and communicate malfunction scenario information and statistics--and in particular that it be used in lieu of the Common Cause Failure terminology.

## 1.0 CLASSIFICATION SYSTEM

### 1.1 Introduction

Within the nuclear industry, events involving multiple component malfunctions due to a shared cause have received a variety of labels with terms such as "common-cause" failures or simply "dependent" failures being frequently employed. Although many authors have investigated and estimated the impact of shared-cause phenomena under one or more names, the results are frequently confusing in the absence of a uniform definition. The authors of this study, under EPRI sponsorship, undertook the task of formulating a working definition for common-cause failure; however, they discovered such a wide diversity of event scenarios that no simple definition proved to be helpful. The alternative was to devise a Classification System for all event scenarios, including the ability to handle shared-cause phenomena.

The development of this Classification System has been an evolutionary process with a number of deliberate steps taken to acquire the views and expert inputs of those concerned with the common-cause failure issue. Initially, in mid-1982, a Survey and a Workshop were conducted to solicit information of a basic nature as related to shared-cause phenomena (e.g., How do you define "failure"?). Assessment of this information led to an early version of the Classification System that was subjected to a comprehensive Data Benchmark Test (DBT) during the latter half of 1982. Six organizations were given a compilation of 50 actual event scenarios to classify according to the instructions provided. The results were assessed to estimate the degree of consistency with a reference solution (which was quite good) and to rectify the reasons for inconsistency. Another group of six participants was provided an improved Classification System for a second DBT conducted in 1983. The present Classification System is the result of an evaluation of these two DBTs, the comments of the twelve participants, and inputs provided by other interested parties including the NRC. References (1) and (2) describe much of the evolution in detail.

### 1.2 Objectives

The objectives of pursuing this line of investigation are three-fold:

- Develop a logical and credible method for dissecting and understanding actual component malfunction scenarios in order to identify multiple events where the shared-cause phenomenon is important;

- Support various analyses, including Probabilistic Risk Assessments, with a means of segregating data for use in statistical and modeling evaluations, and

- Assess the effectiveness of defensive strategies that may be employed where a need to reduce the impact of shared-cause malfunctions exists.

## 1.3 Definitions

One critical stumbling block to a more universal understanding of malfunction scenarios, as indicated by the Survey/Workshop, is confusion as to the meaning of the word "failure". To some analysts, a component failure means physical impairment; the component cannot function until it is repaired or replaced. To others, failure means simply that the component cannot perform its function; the component might be broken or it might require restoration of a proper input or support function such as power, a command signal, or manual reset. Clearly the second use includes the first, leading to confusion. The authors of this paper have restricted the use of the word "failure" to components that are broken, or that require "hands-on" attention. Those components that malfunction due to a lack of proper input or support function are called "functionally unavailable". The general class, which contains both types, is called "unavailable." These mutually exclusive definitions of failure and functional unavailability allow component states in each event to be described in unambiguous terms, while the general population of malfunctions can be more precisely described as "component unavailabilities." All component unavailabilities can be attributed to a cause which, in turn, can be described in two ways: those attributed to another component unavailability and those attributed to root causes. A component cause may be either a failed component or a functionally unavailable component. A root cause is a fundamental defect or condition which may be internal to the unavailable component (e.g., bad piece part) or it may be external (e.g., high humidity), except that it is never another component.

## 1.4 Cause-Effect Logic

Having established clear distinctions between root causes and component causes and between failures and functional unavailabilities, an event scenario can be readily broken down into its elementary parts where each component has an identifiable cause of its unavailability and each scenario has an overall classification. The heart of this system is a diagram depicting the interrelationship between causes and components: such a diagram is called a Cause-Effect Logic Diagram.

Each event scenario can be represented by a Cause-Effect Logic Diagram using a simple set of five logic symbols as shown in Figure 1. The symbols within circles represent causes or cause related information. The symbols within squares represent unavailable components in their recognized states of unavailability.

## 1.5 Unit Classification

In use, the logic symbols are interconnected, left to right, cause to effect, or cause to component. The simplest scenario is called a unit and starts with a Root Cause (circle) connected to a cause node which is connected to a single failed component. This unit is called a Root-caused Failure (RF). If the affected component is functionally unavailable, the unit is called a Root-caused (functional) Unavailability (RU). Similarly, a single component failure or functional unavailability caused by another component is called a Component-caused Failure (CF) or Component-caused (functional) Unavailability (CU).

Two or more components may share a single cause in which case the cause is called either a Shared Root-cause or a Shared Component-cause, the latter being two or more components affected directly by a single component cause. Each of the affected components may be either failed or functionally unavailable. The distinction between Root-caused and Component-caused, between Failure and Functional Unavailability, and between Single and Multiple form a basic set of eight classifications for the units which are shown in matrix form in Figure 1-2.

### 1.6 Event Classification

Malfunction scenarios are frequently complex, and may involve a number of components that are interconnected in various ways. Such a sample scenario is depicted in Figure 1-3.

In this example, there are four nodes and four units, each shown encircled. Unit 1 (including cause node 1) is classified as an RF, unit 2 as a SCF, unit 3 as a CF, and unit 4 as a SCF. Note that components 1, 2-1, and 2-2 are linked; that is they appear in two units, once as the affected component and once as the component-cause.

Events, such as those illustrated by Figure 1-3, can also be classified (see Figure 1-4). The resulting Event Classification follows the dominant unit classification that makes up the event. For example, some events progress linearly, from cause to affected component and sometimes on to additional components. These events are classified as Linear Single (LS) if only one component is involved, and Linear Multiple (LM) if there is more than one component in the chain.

Other events are characterized by a branch typical of the shared-cause units. A cause-effect logic of an event containing a single branch point classifies the event as a Root-caused Branched Single (RBS) if the branch point is either an SRF or an SRU. If the single branch point is either an SCF or an SCU the event is classified as a Component-caused Branched Single (CBS). Multiple branch points within an event scenario are also possible. When all these multiple branches are component-caused (such as shown in Figure 1-3), the event is classified as a Component-caused Branched Multiple (CBM). If there is a mixture of component-caused and root-caused branches, the event is classified as a Mixed-caused Branched Multiple (MBM).

### 1.7 Additional Discussion

By using the simple symbols in Figure 1-1, complex event scenarios can be represented as elementary interconnected cause-and-effect relationships. The Cause Node, in particular, allows flexibility in handling multiple cause situations. For example, if an event has two causes and both are necessary, the cause node circle is inscribed with a 2/2. If there are three causes present and any two of them are sufficient to cause the effect, the inscription is simply 2/3.

The cause-effect logic also allows the designation of potentially failed components by use of the inscribed PX symbol. A potential failure is one that falls short of actual failure but there must be evidence of an incipient failure mechanism in place, or evidence of an actual exposure to a cause of failure known to have failed similar components under similar circumstances. A clear distinction is made between hypothetical events and potential failures. Hypothetical events are based on conjecture, unsubstantiated evidence, or imaginary scenarios. Accordingly, potential failures, as defined, are acknowledged; hypothetical failures are not.

Actual malfunction events, duly recorded for retrieval, provide the basic data from which various analyses of operating experience are derived and applied. These recorded scenarios are, by definition, system specific since they represent events on well-defined systems or plants under circumstances peculiar to the time and factors surrounding the event. Since data applications of interest will not usually correspond to the system specifics from which the data derives, it was clear that cause-effect classification was necessary at the component (black box) level of assembly. With the necessary descriptions of component unavailability, analysts could use such data across a virtually limitless array of systems. As discussed previously, this created the "Unit Classification." However, recognizing the need to understand the proper context of the entire scenario also led to the "Event Classification." As a rule, the more complex malfunction scenarios are composed of two or more "Units" to describe the total "Event" while the simplest unavailability scenarios require a single "Unit" to describe the "Event."

### 1.8 Categorization Information

The Classification System provides a logical method for the dissection and reconstruction of malfunction scenarios, and the identification of their constituent elements in a concise and unambiguous terminology. Beyond that, there is a considerable array of sub-tier information that might be additionally extracted from an event report for use by analysts. This extraction process is herein referred to as Categorization. It is, by its very nature, independent of the Classification System, and is intended to focus on characteristics of the events that are important to both managers and technicians. Categories range from simplistic information (e.g., date of event, plant identification, etc.) to sophisticated information (e.g., cause, method of discovery, corrective action taken). Primary attention is usually focused on the individual components in the event (e.g., failure mode) but attention is also given to the event as a whole (e.g., method of discovery). In Section 2.0 of this paper, four categories are specifically discussed.

## 2.0 DATA ANALYSIS

### 2.1 Introduction

A comprehensive data analysis activity has been implemented using the Classification and Categorization System described in Section 1.0. Currently, Nuclear Power Plant Licensee Event Reports (LER) constitute the data source for this activity. This paper will report on some selected analyses conducted with LER data on the Auxilliary Feedwater System (AFWS) in operational Pressurized Water Reactor plants.

The data base employed here draws upon all LER's from the period 1973 to 1983 inclusive - a total of 635 separate (independent) events, extracted from 604 LER's.

The objective of these analyses is to identify trends and characteristics of the AFWS event population that might be important indicators of further investigation. It is also intended that these analyses will help to provide an input and clarification to the use of such data in the various statistical and modelling analyses that are conducted throughout the nuclear power community.

### 2.2 Event Classification

The 635 AFWS events were initially classified according to the system shown in Figures 1-2 and 1-4. The classification results are shown on Figures 2-1 and 2-2. All values are shown as a percentage of the total population of 635 events. The diagrams presented represent the Event Classification structure format; the superscript abbreviations above each component box indicates the Unit Classification assignment to which they belong and the subscript shows the actual count from the data base.

Observations of note include the following:

- Linear events dominate the population by about 8 to 1.
- Only 2% of the population are Linear Multiple events consisting of failures only - i.e., the classic "domino" or cascade event. Cascade failures, apparently, are infrequent occurrences in the AFWS.
- While Branched events are 11% of the population, those involving multiple failures are only 6% of the population. Of those, all events are SRF's except for one SCF.

There are some interesting implications in these statistics relative to multiple unavailability data and how they might be used in estimating parameters such as B or C factors. For example, should all Branched events be used in the numerator (i.e. failures, functional unavailabilities and potential failures) or just Branched failures? As these statistics show, the answer could change the numerator by a factor of almost 2X. Should the Linear Multiples be included? If so, the numerator can vary by a factor of almost 6X (versus the use of Branched failures only). This analysis explains, in part, why we see such a wide range of ß factor values in the literature; authors rarely reveal the details of their data base, and it would appear from the above observation that selections include a variety of combinations from the Linear Multiple and Branched events. It is suggested that use of the Classification System by analysts would not only clarify the origin of their statistics, but also considerably narrow the apparent disparity of their published results.

## 2.3 Root Cause

Each event is initiated by some root cause - i.e., some basic underlying deficiency or problem. Ideally, a root cause would be identified with a level of detail such as failure mechanism or physics of failure phenomena. LER information rarely allows for such detailed analysis. Hence, root cause in this paper is identified at a fairly top-level description such as Design, Human Error, etc. The LER information is almost always sufficient to allow an accurate determination of this top-level description of root cause. The root cause results are shown in Figure 2-3.

Observations of note include the following:

- The dominant root cause of Linear events is entirely different than that for Branched events - i.e., Internal (e.g., piece part/wiring failure, material flaw, etc.) versus Design, respectively. This is potentially very significant information. In the case of Branched events, it implies that a major element of defensive strategy resides with the basic design concepts that may be employed, but more detail is needed to know what that might be, or how operational actions might be employed in lieu of design change actions in existing plants. With the Linear events, it also tells us that more information (perhaps detailed failure teardown/analysis) is needed before we can ascertain what, if anything, can be done to further prevent the Internal-caused events.

- Human Error (on the part of operator or maintenance personnel) is relatively small for both Linear and Branched events. Frequently, Human Error is said to be a major proportion of the root cause problem. Here, this does not seem to be the case.

## 2.4 Components Involved

Each box shown in Figures 2-1 and 2-2 represents unique components that were involved in the events. (Components, herein, are a black box or its equivalent and would include equipment such as pumps, valves, pipe supports, pipe sections, switch boxes, etc.). The distribution of these components is shown on Figure 2-4.

Some observations on these statistics include the following:

- Pumps and valves clearly dominate the Linear events (80% of total).

- In the Branched events, pumps, and valves are joined by other mechanical equipments to dominate the component list (77% of total).

- However, instrumentation components proportionally increase markedly in the Branched (vs Linear) events. Nonetheless, mechanical components still dominate both areas.

- This information, coupled with the root cause information of Figure 2-3, may prove useful in the formulation of defensive strategies (a study that is currently being initiated).

## 2.5 How Detected

The method of problem discovery, and the general interest in how it occurs and may be improved, is of vital interest to operation personnel. This information for the AFWS is shown on Figure 2-5.

Observations on these statistics include the following:

- Actual AFWS operation (as opposed to other forms of surveillance and off-line checkout) is the most prominent means by which these events were discovered. It is probably reasonable to observe that plant operators would prefer this not to be the case!

- Routine tests/inspections do a commendable job - but is it enough? And how might it be improved?

- Apparently, special tests/inspections and information supplied to plants from other external sources are useful in detecting Branched events. This might suggest the need for more attention to "special" tests and the importance of "anomaly information exchange" between plant sites - a task that INPO (Institute of Nuclear Power Operation) is promoting in the USA.

## 2.6 How Corrected

Each LER contains information regarding the corrective action taken as a result of the event occurrence. Generally, one would tend to assume that corrective actions reflect some positive correlation with the root-cause of the event. The results of this analysis, shown in Figure 2-6, offer some interesting insights in this regard.

- As expected, "Replace plus Repair-Same Hardware Design" are the majority corrective actions (61%) for Linear events (recalling from Figure 2-2 that "Internal" is the leading root cause of Linear events). However, these two categories also represent 35% of the corrective actions for the Branched events, and this may not be the appropriate solution for this class of events.

- In the Branched events, however, "Replace-Different Design" is more frequent than with the Linear events, suggesting that some level of positive recognition is being given to the prevention of Branched events (recalling, from Figure 2-2, that "Design" is the leading root cause of Branched events).

- While "Human Error" as a root-cause was not large, as previously noted, it is interesting to note that "Training" as a corrective action is virtually nonexistent.

- "Procedure Change," when viewed on an event-by-event basis, frequently does not correlate with "Procedure" as a root cause (i.e., "Procedure" caused events often take other forms of corrective action). Conversely, "Procedure Change" is frequently the form of corrective action when "Procedure" is not the root cause. While this may be appropriate, the reasons for this are the subject of further study.

- The Branched events show a sizeable tendancy (17%) toward "Planned" (longer term) corrective action. This may reflect the consideration of hardware design or procedure changes that were under evaluation when the LER was written.

- When corrective action was "Not Needed/Not Possible" (Linear events only), this could indicate the lack of sufficient detail in the causative information for plant personnel to define specific corrective actions.

## 3.0 CONCLUDING REMARKS

A logical method for the dissection and reconstruction of malfunction scenarios has been described. This method - the Unit and Event Classification System - permits the identification and communication of the constituent elements of these scenarios in a concise and unambiguous terminology. It facilitates the analysis of operating event data which was illustrated via its application to AFWS event reports. The AFWS analysis also examined four categories of sub-tier information to indicate the type of insight that can be additionally extracted from such reports.

More specifically, it is suggested that the "Common Cause Failure" (and related) terminology be dropped by the technical community, and that the Unit and Event Classification System be used as a universal means of interpreting and communicating malfunction scenario information, models, statistics, and operating experience in general.

## 4.0 REFERENCES


1. Crellin, G. L., Jacobs, I. M. , and Smith, A. M., "A Study of Common Cause Failures, Phase I: A Classification System," EPRI NP-3383, January, 1984.

2. Crellin, G. L., Jacobs, I. M., Kujawski, E., and Smith, A. M., "A Study of Common Cause Failures, Phase II: A Comprehensive Classification System for Component Fault Analysis," EPRI NP-3837, January, 1985.

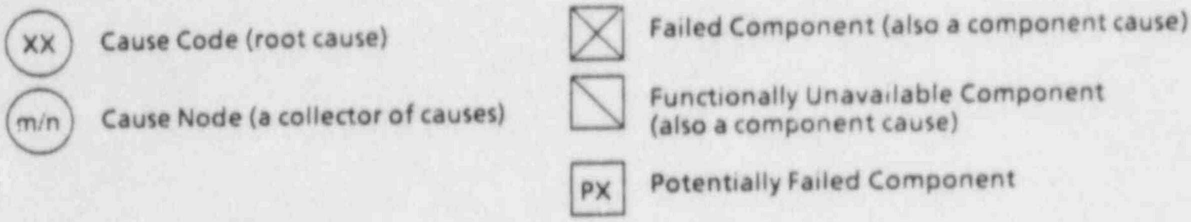


Figure 1-1 Cause-Effect Logic Symbols

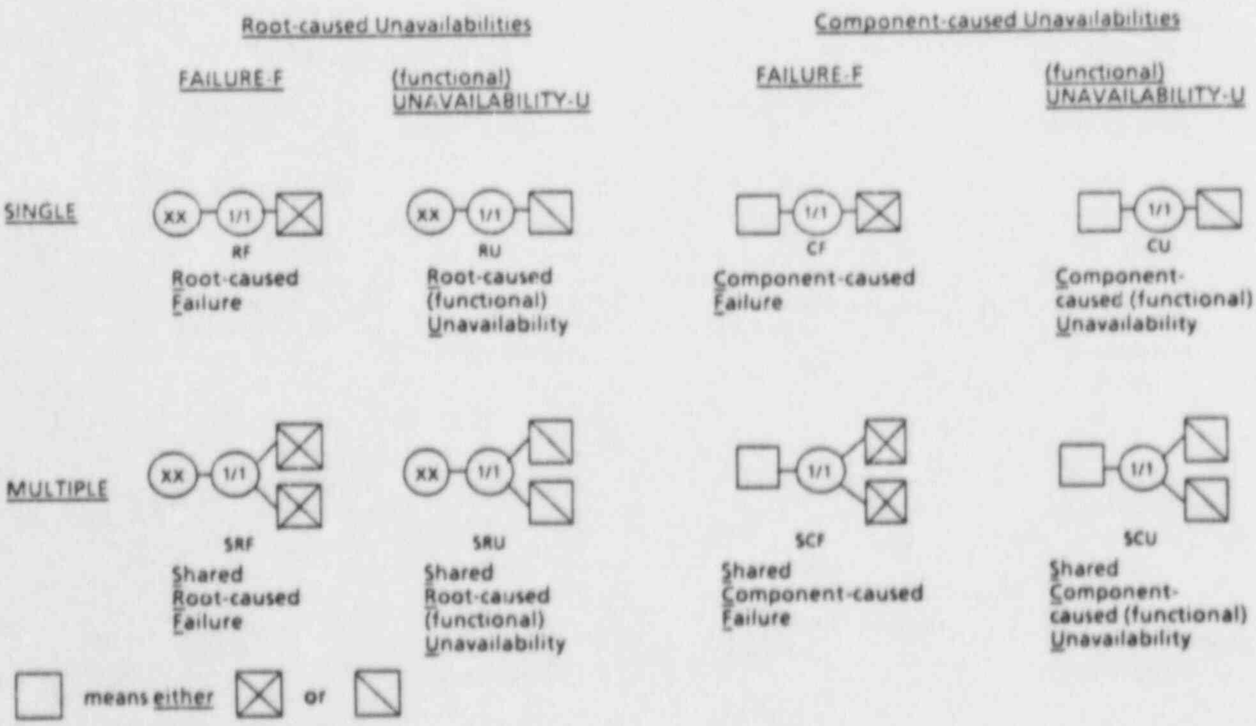


Figure 1-2 Basic Classifications of Cause-Effect Logic Units

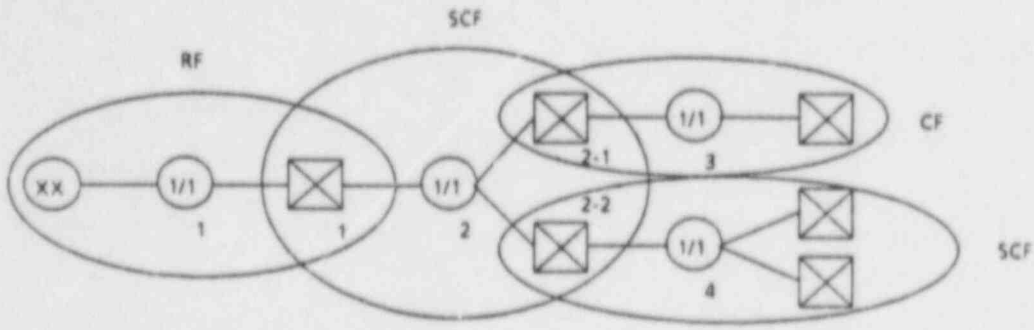


Figure 1-3 Sample Complex Scenario

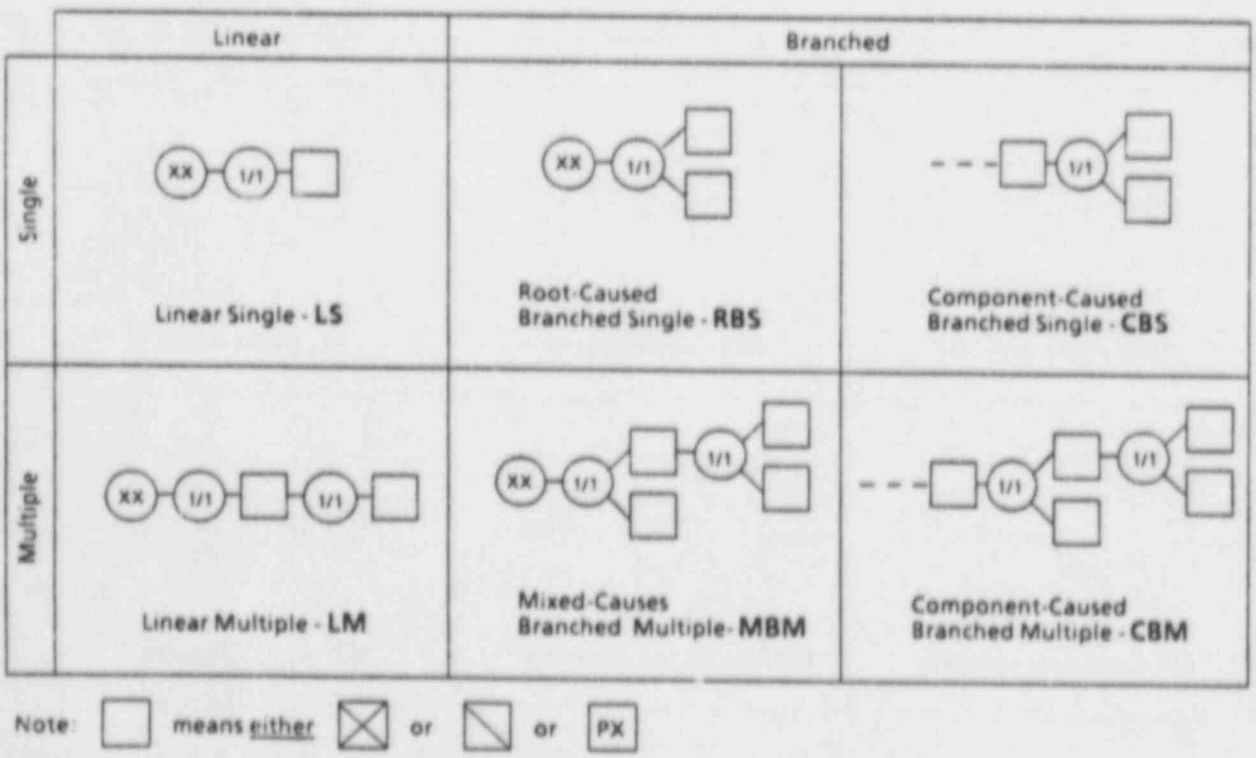


Figure 1-4 The Event Classification Hierarchy

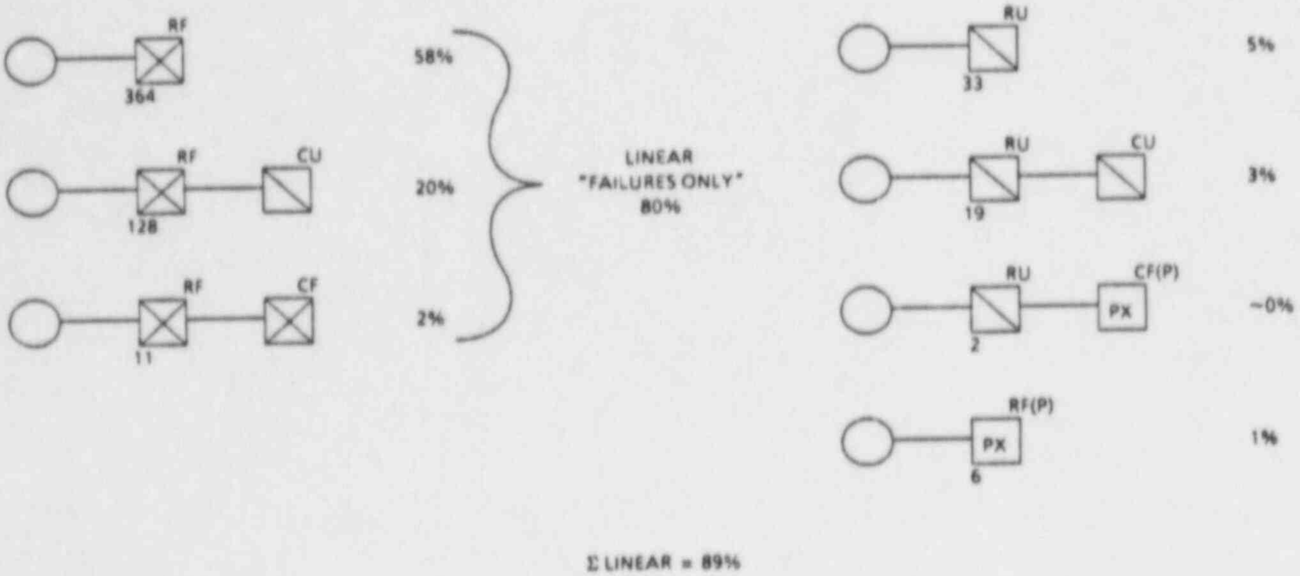


Figure 2-1 Summary-Linear Events

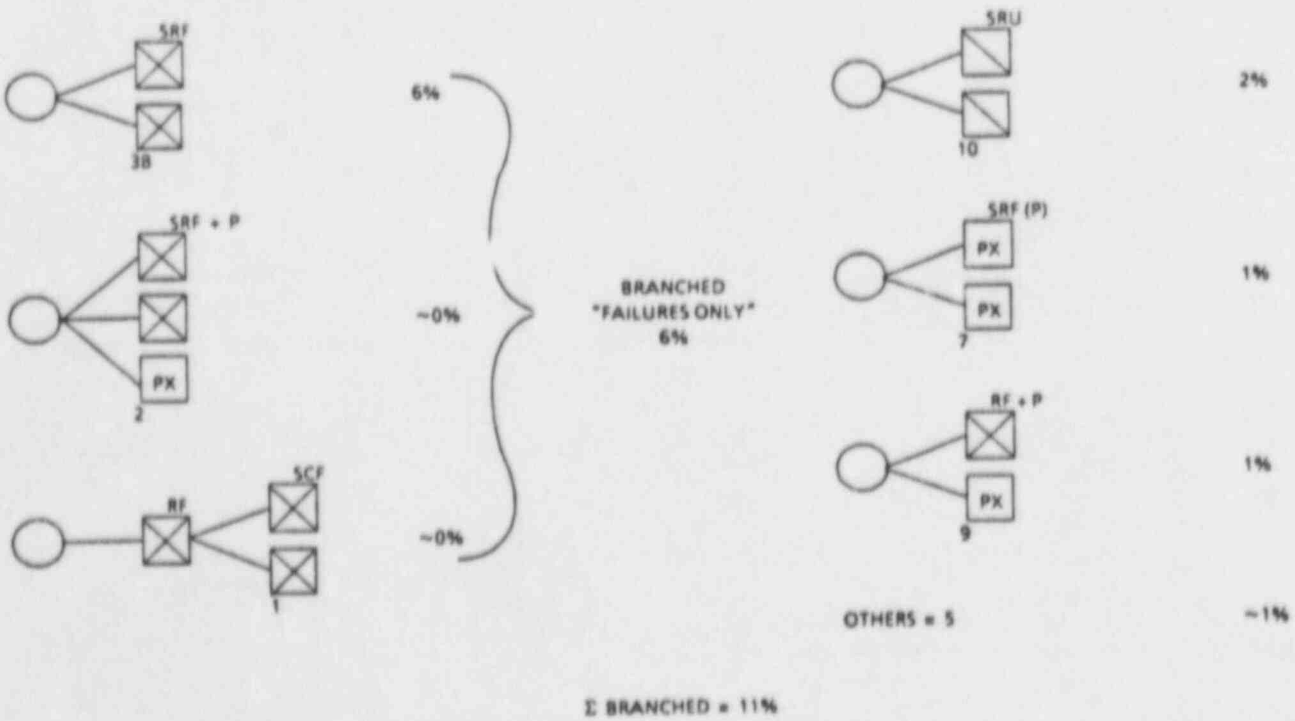


Figure 2-2 Summary-Branched Events

| | LINEAR | BRANCHED |
|---|---|---|
| DESIGN | 9% | 50% |
| PROCEDURES | 6% | 11% |
| HUMAN ERROR | 12% | 17% |
| INTERNAL | 57% | 6% |
| ENVIRONMENT | 5% | 15% |
| OTHER | 11% | 1% |

Figure 2-3 Root Cause

| | LINEAR | BRANCHED |
|---|---|---|
| PUMPS | 48% | 31% |
| VALVES | 32% | 27% |
| INSTRUMENTATION | 9% | 21% |
| OTHER - ELECTRICAL | 5% | 2% |
| OTHER - MECHANICAL | 6% | 19% |

Figure 2-4 Components Involved

| | LINEAR | BRANCHED |
|---|---|---|
| OPERATION | 49% | 42% |
| ROUTINE TEST/ INSPECTION | 40% | 31% |
| SPECIAL TEST/ INSPECTION | 4% | 13% |
| EXTERNAL SOURCE | 2% | 10% |
| UNKNOWN/OTHER | 5% | 4% |

Figure 2-5 How Detected

| | LINEAR | BRANCHED |
|---|---|---|
| SAME HARDWARE DESIGN | | |
| - REPLACE | 21% | 8% |
| - REPAIR | 40% | 27% |
| - PROCEDURE CHANGE | 9% | 20% |
| - TRAINING | 4% | 0% |
| DIFFERENT HARDWARE DESIGN | | |
| - REPLACE | 6% | 15% |
| PLANNED | 5% | 17% |
| NOT NEEDED/NOT POSSIBLE | 8% | 0% |
| NONE REPORTED | 3% | 2% |
| FIX ANOTHER COMPONENT OR SYSTEM | 4% | 11% |

Figure 2-6 How Corrected

Safety Research in Transition:
From Accident Description to Accident Prevention & Accommodation

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## 1. Introduction

In thirteen years, much has changed in safety research. The changes reflect the changes in the industry. From a massive construction program, there has been an emergence of a large operational experience base in the utility industry. The lessons learned have been profound and reflect the emergence of operational safety, emphasizing the control and management of accidents and transients. The focus of the research efforts is to identify and reduce the areas of high risk - societal and economic - for today's generation of LWRs. From this basis, we believe will also emerge many features for new plants.

There have been clear changes in safety research and direction, and this is becoming more apparent as the technology and results are focused on power plant applications. Traditionally, safety research has emphasized detailed analysis and description of reactor accidents, from defined transients to design basis accidents (LOCA), and events beyond the design basis. The tools to perform such analyses are now approaching an operational maturity. The technology is in wide use in the utility industry and significant safety and operation margins are perceived. Current EPRI methods also handle almost all the required Safety Analysis Report Chapter 15 transients (see Tables 1 & 2). Successful application of RETRAN to plant operational transients has been well documented (1) and revisions to design basis analyses are now under active NRC discussion.

The extension to beyond design basis events, including degraded cores, shows that the dominant interest is in the quantification of the potential radioactive source term. Siting and emergency planning aspects are the important implications.

What the analyses show is that there is great opportunity and potential to avert accidents and consequences by preventing and accommodating accidents. This also reduces the economic exposure of the industry, and reduces the occurrence and consequence of significant plant events.

The other important concept is that much can be done to assure accommodation of accident consequences, either by demonstrating that significant safety margins exist in the design, or that by actions and procedures significant problems can be averted.

This transition from "classic" to "modern" safety research is subtle but with profound implications. We can approach this opportunity at each level:

- reducing or eliminating the perceived or real accident initiators;
- enhancing and refining the means and methods of monitoring, diagnosis and control of accidents should they occur;
- eliminating and reducing the consequences of accidents by plant accommodation or recovery, or both.

The benefit is then not only reduced risk, but enhanced reliability and availability. A well run and controlled plant is a safe and economic plant.

The ways effective accident prevention and accommodation can be achieved are many, and in marked contrast to traditional safety analysis:

- reduction in predicted risk due to seismic events, by realistic and quantitative definition of the margins inherent in plant design;
- emphasis on reliability-centered maintenance with a thorough knowledge of plant and equipment status;
- reduction in safety challenges and trips by upgrading control and safety systems where demonstrated to be needed;
- recognition and removal of common mode events as potential accident initiators;
- improved accident and transient diagnosis by operator information systems and signal validation, including the potential of utilizing validated expert systems;
- enhanced transient control by integrating emergency guidelines, and by quantifying the influence of operator actions and reliability;
- examining plant recovery strategies for all stages of accidents and transients;
- utilizing source term studies to quantify those transients and situations of major risk and the areas of potential risk reduction;

The safety research underway at EPRI is examined in the context of this transitional phase and revised objectives, and in how the application to U.S. plants is being explored in conjunction and cooperation with U.S. utilities. This paper emphasizes the recent results since the last Information Meeting, and assumes some familiarity with the earlier reported EPRI work (2). A Bibliography of the recent publications is also included.

## 2. Accident Prevention: Elements and Models

As noted above, we have the three elements:

- initiation
- control
- recovery

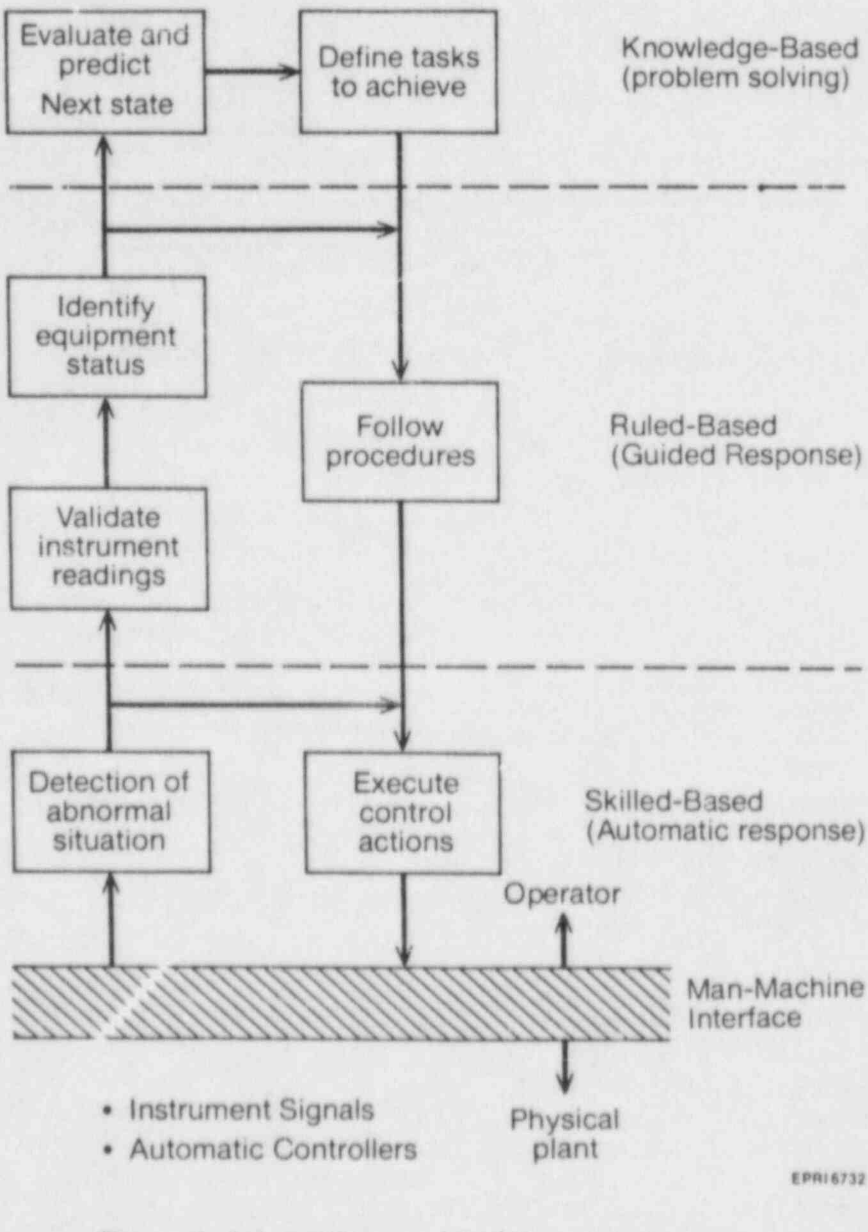


Figure 1. Information model of the control process.

Common-cause initiator dependencies due to so-called external events, such as fire-or earthquake-induced sequences, generally rank among the top severe core damage sequences for those studies which include such analyses; for example, fire sequences contribute 9% of total core damage frequency for Zion, 24% for Big Rock Point, and 15% for Limerick. One obvious conclusion from this observation is that it could be misleading to compare the total core damage frequency for a study which did not include external events to that of a study that did.

As we have noted elsewhere (3), the use of computer-aided technology for accident prevention and control has been widely deployed by the utilities in the post-accident response to the Three Mile Island (TMI-2) nuclear incident which occurred on March 28, 1979. Major lessons learned, both from the TMI-2 accident and a host of probabilistic safety evaluations, suggest that human error is a significant contributor to the overall plant risk. Multiple events and failures that are seemingly minor in nature could be combined accidents of different severities if human or technological diagnosis results in imprecise control of the events.

The utility motivation is to reduce plant risk, coupled with the effort of providing capability to meet government regulatory requirements for Emergency Response Facilities, and the incentive to obviate technological obsolescence of some plant systems. The result is significant impetus for the application of computer-aided technology for plant operations and control.

## 2.1 Information Model of Control Process

The recent effort on the prevention and control of accidents is closely coupled to the control process in the nuclear power plant. Figure 1 is an illustration of an information model of the control process in a nuclear plant control room. The man-machine interface control board separates the (human) operators from the automatic controllers and instrument signals. The control process of a human operator has been generally categorized into three stages of increasing complexity: (1) skill-based automatic response; (2) rule-based guided responses; and (3) knowledge-based problem solving. The boxes represent in simple terms the state of mind and the action an operator takes in the different stages of control. Current practices for operator training emphasize stages (1) and (2).

As the NRC (AEOD) report stated, there are many incidents, such as failures and errors, which can lead to initiation of transients and events in various parts of a complex nuclear plant. Information from these events may not always reach the control room immediately. The control room crew relies primarily on the sensor readings and alarms to provide them the instantaneous status of the plant.

The TMI-2 accident demonstrated that complex multiple events, beyond skill-based and rule-based control, could degenerate into a severe accident. The operating crew was not trained to recognize and control the accident properly; the system was not accommodating to the event severity. The goal in safety control and accident management is to develop technologies to aid control room information and knowledge processing, and to offer fault-tolerant control and instrumentation systems so that the transient initiators and challenges to the plant can be reduced (4). This would support the operating staff to

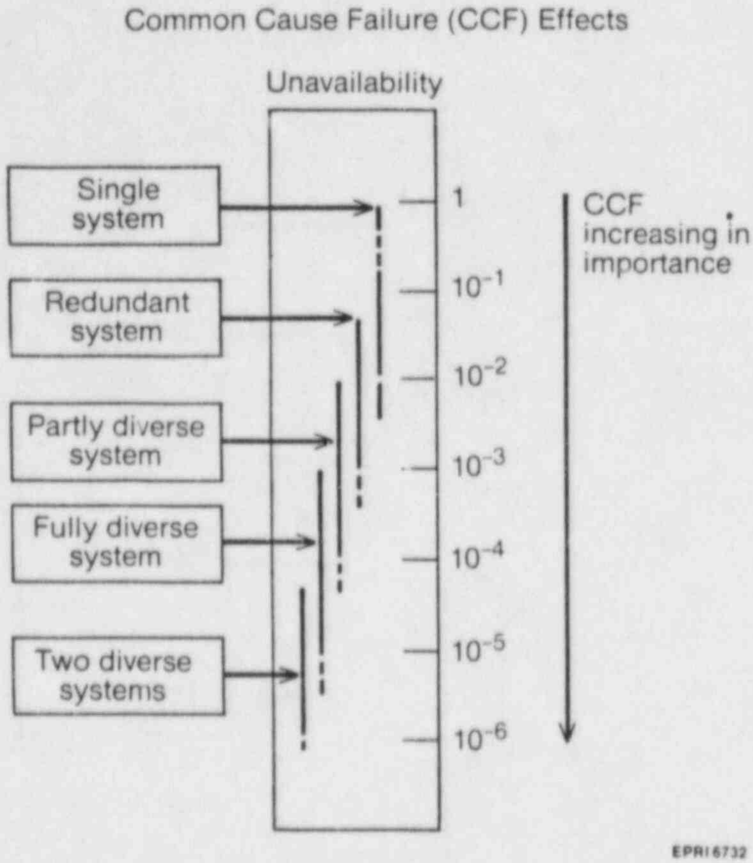


Figure 2. Importance of common cause failures.

simultaneously diagnose the malfunctions and initiate the proper corrective actions expeditiously. Utilization of symptom-based procedures reduces the emphasis on diagnosis, however, to properly develop the safety control technology, one must meld together the disciplines of probabilistic risk analysis, formal sequence description, equipment and human reliability evaluation, computer-based information, diagnosis and control systems, and phenomenological exploration of the physical processes occurring in plant operations, transients, and accidents. Note that only the latter area figured heavily in the early days of "classic" safety research.

3. Initiating Events

Current research examines two classes of important initiating events:

(1) external events such as seismic forces which may challenge the structural and functional integrity of the plant and lead to compounding failures;

(2) internal events, such as off-normal plant operations, common cause failures and plant trips, which may challenge the functional capability of control and safety systems, and constitute challenges to plant safety systems and setpoints.

Indeed common cause effects become of increased significance as the reliability of the other system is improved (Figure 2).

3.1 Earthquake Hazard and Design

The two important questions addressed by EPRI's R&D are:

- what are the true likelihoods of significant threatening seismic events? (The seismic hazard.);
- what are the inherent and designed structural safety margins? (The seismic margin).

3.1.1 Seismic Hazard

Nuclear power plants are designed and built to withstand major earthquakes. Seismic design is a major factor (9-15%) in the cost of constructing and licensing the plants. These cost percentages assume that the engineering and installation is done only once. More typically in recent years, the instability of the licensing process has caused changes to occur during design, construction, and pre-operation periods, increasing earthquake protection costs. Finally, for operating plants, there are recurrent concerns (5) about their seismic adequacy and expensive evaluations and retrofits being done. The underlying reasons for these extraordinary activities are: (a) developing insights on earthquake processes and (b) a sparse data base for many, if not most seismic design aspects. The former has lead to a change in the technical position of the U.S. Geological Survey (USGS) a requirement by the U.S. Nuclear Regulatory Commission (NRC) for a reassessment of seismic design bases of Eastern U.S.A. (EUS) plants based upon seismic hazard estimates in the United States. The latter has lead over the past 20 years to a body of design practices and regulatory requirements which are individually conservative and, being uncoordinated, lead to extraordinary and unquantified conservatism and excessive costs for the safety being achieved.

Recognizing this important issue, EPRI formed a Seismic Center in 1983. The Center is the focal point for EPRI research supporting the resolution of seismic issues and the development of advanced technology and enhanced criteria for nuclear-related facilities. The Seismic Center has a Technical Advisory Panel of distinguished earthquake specialists and an advisory group of utility engineers. At this time, the research is focused primarily on four issues:

- assessment of seismic hazards within the EUS;
- evaluation of seismic margins for EUS plants;
- generation of a data base for soil-structure interaction;
- generation of a data base for piping damping and ultimate capacity.

### 3.1.2 Reassessment of Seismic Hazard Within the Eastern United States

When thinking about earthquakes, most people think of California. They are generally unaware that some of the largest earthquakes within recorded U.S. history occurred east of the Rockies. Assessment of seismic hazard in the EUS is much more difficult than in the west because the lower occurrence frequently results in a smaller historical record, and the underlying causes are less apparent. Earthquakes in California are caused by the relative movement of the Pacific and North American tectonic plates. The periodic interplate slippage allows relatively accurate prediction of the location and frequency of future earthquakes. The intraplate (within the North American plate) earthquakes in the EUS are of a different character.

Past practice in siting most existing nuclear plants has involved an assumption of geographic stationarity for large earthquakes and a demonstration that such large earthquakes are associated with local tectonic structure has been required (e.g., 10CFR100, Appendix A, 1974). However, the historical record continued to strongly influence regulatory decisions. If an area had experienced a large earthquake, (e.g., as at Charleston, SC, in 1886), it was assumed that some local geologic structure was the cause even when the structure was unknown. Increased understanding of earthquake processes has undermined this practice, and the USGS altered its view relative to the more traditional practice. As a result, the NRC is reassessing the seismic hazard at nuclear sites in the EUS.

The objective of the EPRI program is to achieve, by working with the NRC, a stable basis for seismic hazard estimation for the EUS. The EPRI approach has benefited greatly from the pioneering NRC-sponsored work at Lawrence Livermore Laboratory (LLL) has introduced several improvements including:

- use of six independent Tectonic Evaluation Contractors;
- the generation of a comprehensive geotechnical data base for the EUS;
- the use of extensive peer review;

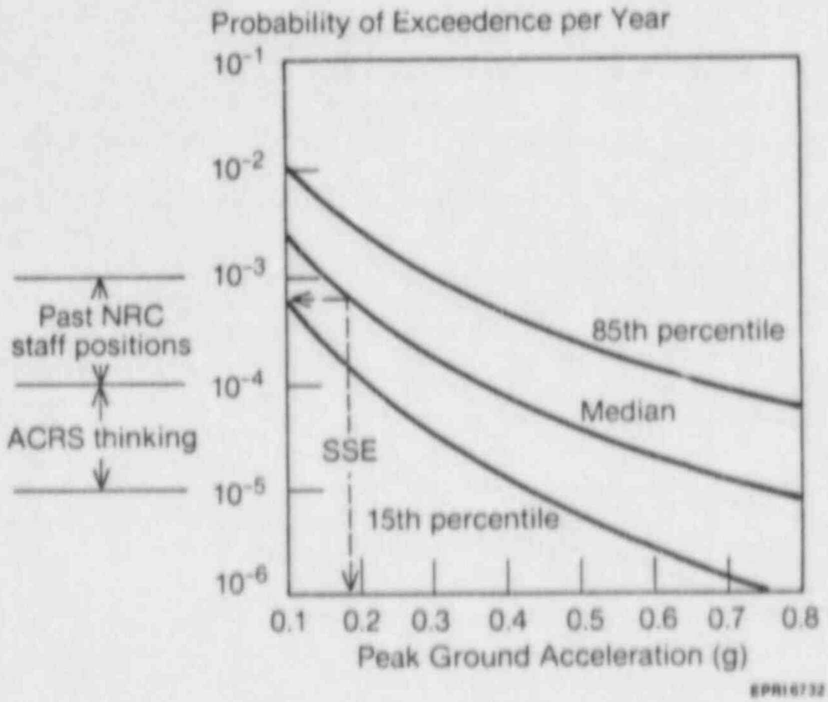


Figure 3. Seismic hazard curve for representative EUS plant.

and the implementation of a traceable and scrutable methodology encoded in a computer program meeting quality assurance requirements of 10CFR50, Appendix B.

It is expected that, by January 1987, the utilities will have an acceptable methodology for assessing the seismic hazard at sites within the eastern United States. By having a technically-supported stable basis for determining the seismic design, one source of licensing delay should be eliminated.

### 3.1.3 Evaluation of Seismic Margins for EUS Plants

The preliminary LLL report (6) reassessed the seismic hazard for ten sites. As illustrated in Figure 3 for a representative site, the new NRC methodology suggests that their existing design bases Safe Shutdown Earthquake (SSE) have an exceedance probability of about $10^{-3}$ to $10^{-4}$ per year. The Advisory Committee on Reactor Safeguards (ACRS) has been suggesting that earthquakes with an exceedance probability in the range of $10^{-4}$ to $10^{-5}$ per year should be considered. Therefore, some plants within the EUS may be required to quantify seismic margins beyond the design basis embedded in the plant. Anticipating this potential NRC requirement, the electric utilities desire to develop an economical approach with well-defined analysis to minimize unnecessary retrofit to satisfy such a reevaluation.

In order to obtain input from a broad spectrum of the technical community, EPRI and the NRC co-sponsored a workshop on seismic reevaluation in October 1984. Publication of the proceedings (7) is imminent. The participants generally agreed that substantial conservatism exists in the seismic design of nuclear plants which implies large seismic margins.

From a review of probable risk assessment (PRA), it was found that all components important in the plant risk analysis were approximately twice the plant design SSE and median ground accelerations before failure. Thus, the studies confirm that current seismic design practices for nuclear power plant structures and equipment are very conservative. Also, these PRAs have shown that, in particular, shear walls and piping possess consistently high seismic capacities; therefore, seismic margin reviews need not concentrate on these components.

Substantial data on the dynamic capacity of equipment and piping has already been gathered from field experience and laboratory experiments. The utility-sponsored Seismic Qualification Utility Group (SQUG) has surveyed the performance of eight classes of equipment directly relevant to nuclear plants (e.g., motor control centers) at fossil-fuel power plants in California during past large ground motions (up to 0.5g). SQUG surveyed the damage to industrial facilities during the Richter 7.7 earthquake in Chile on March 3*. EPRI is supplementing this work by evaluating generic ruggedness based upon existing equipment test data.

*Similar information will be available from the recent earthquake in Mexico.

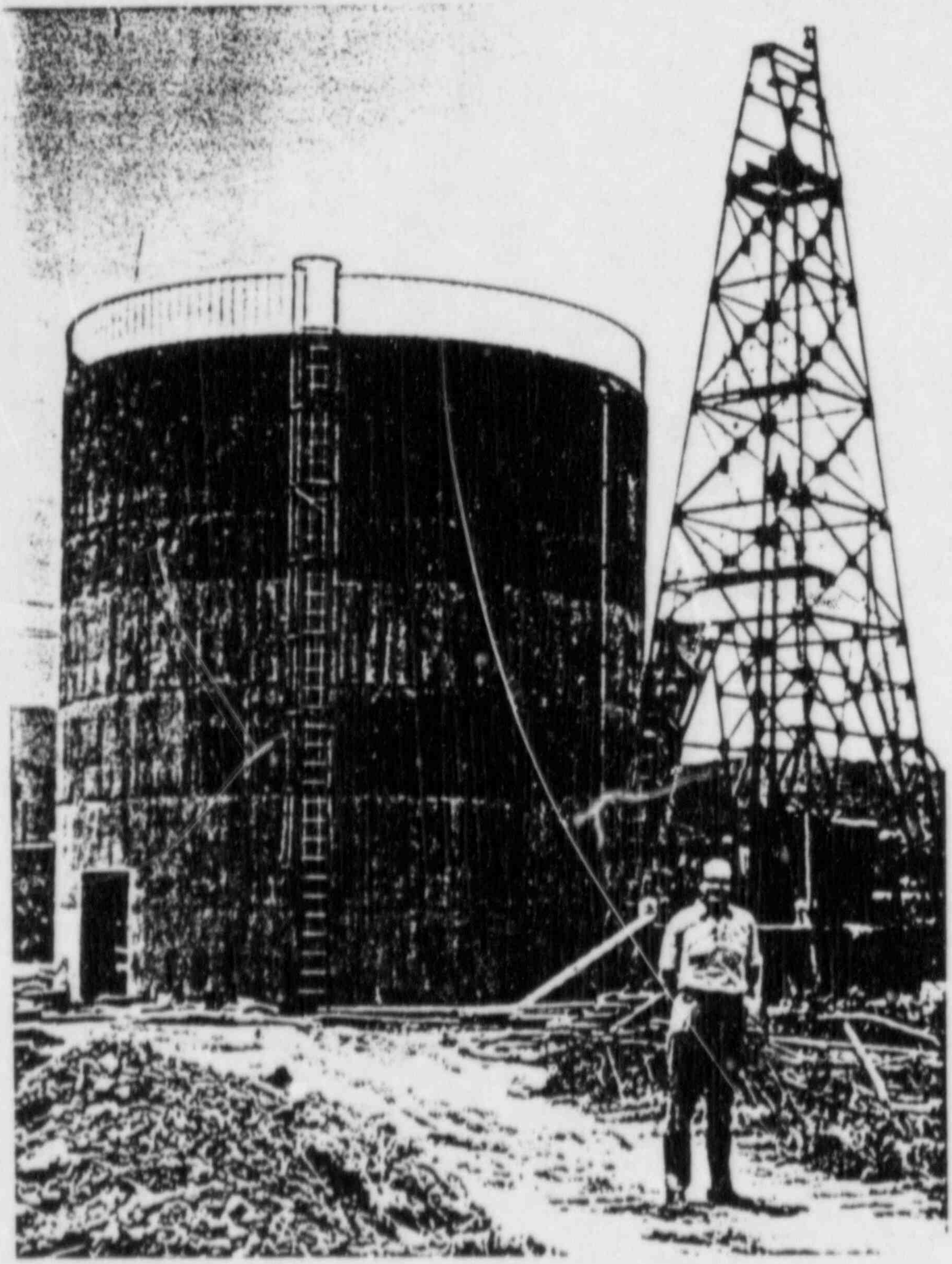

Figure 4. LOTUNG large scale
seismic experiment.

Recent experiments on the ultimate capacity of piping systems including snubbers (8) studied a three-dimensional system of six-inch Schedule 40 piping with internal pressurization of 1150 psig. This work was done in cooperation with the NRC/RES. Such piping can sustain four times the design basis seismic load without leakage, let alone structural failure. Additional experiments are planned subjecting piping systems to larger loads to determine their ultimate capacity and failure mode. Large scale tests on a model containment in an earthquake-prone area are underway in a joint effort with Taipower (Figure 4).

A new project has specific objectives to provide to the utilities procedures, criteria and technical bases for economic evaluation of seismic margin. A detailed walkdown procedure identifies critical components which need more in-depth reevaluation. Not every component, pipe or structure has equal importance to the seismic safety of a nuclear power plant, and it would be uneconomic and non-beneficial to reevaluate everything. It is expected that the seismic PRAs will be used to identify the critical components, etc.; a review (9) has already provided some insight. A detailed procedure for quantifying the seismic margin of the critical components will integrate the results developed in many other EPRI projects. An important aspect of this project is a demonstration of the procedures by application to two existing nuclear power plants. The NRC and EPRI have agreed to compare their respective reevaluation procedures by applying them to the same two power plants.

Despite near-term costs, the above EPRI and related NRC projects should demonstrate the seismic ruggedness of nuclear power plants and hence provide additional assurance of reactor safety for extreme initiating events.

### 3.2 <u>Safety and Control System Challenges</u>

Each time a plant trips, the protection and control systems are challenged. Therefore, an important aim is to reduce trip frequency and also the potentially debilitating failures, such as common mode failures, which dominate as the unreliability and failure potential are decreased (10).

Trips can be caused by simple instrument failures, due to degradation, drift or maintenance problems, or by more subtle interactions which require determining what is a <u>valid</u> signal.

The dominant challenges and source of plant trips (~ 50%) are the feedwater systems (11). These systems also played a significant role in the TMI event. The trips can be at low or high power. Therefore, enhancing the reliability and controllability of the feedwater system, and reducing the failure (trip) potential can serve the dual role of improving plant safety and plant performance. Simple widening of the level trip points avoids the cause and may well lead to more and other primary system challenges.

The existing plants utilize largely analog systems that are no longer produced as main product lines in the electronics industry. This means that the utilities will find it increasingly difficult to obtain parts, maintenance and service. Therefore, the purpose of development of the digital control systems is to provide an option for the potential replacement of the existing aging and obsolete analog control systems and for the reduction of plant challenges.

The immediate goal is to develop digital feedwater controllers for both BWRs and PWRs (12,13). These controllers will utilize microprocessors and software that can detect and isolate sensor failures.

The design of the digital systems for plant applications will consider backfit requirements for the plant and will also account for the plant operation experience to achieve better integration of the interfaces between the primary and the balance-of-plant systems. The specification, reliability and design requirements have all been completed for prototype installation in 1986 and 1987. These systems are designed to markedly reduce the trip initiation rate due to hardware and software.

## 4. Control of Accidents and Transients

The three important aspects of transient control are those of:

- human reliability;
- diagnosis and decision making;
- procedure guidelines selection and execution.

### 4.1 Human Reliability

Although PRA can provide significant insights about the contributions of human interactions in accident scenarios, review of PRA studies (14) have found that there is potential for wide variation in the results, stemming partly from the use of different analysis methods and assumptions relating to human reliability.

The quantification of failure to execute proper response per procedures and training were found to be generally in the range of $10^{-4}$ to $10^{-3}$ per demand among the ANO, Limerick and Zion studies, despite considerable variability in the details of deriving the values. Big Rock Point derived failure probabilities on the order of $10^{-3}$ to $10^{-2}$ per demand for similar activities. Areas that account for much of the observed variability in probabilities are the assumed stress-factor multipliers on basic human error probabilities, the number of operators available in the specific plant and the assumed degree of dependence between operators. However, interchange of human error probabilities for similar actions between ANO and ZION, for example, does not substantially alter core damage sequence frequencies.

The purpose of the SHARP project (15) was to develop a disciplined framework to aid analysts in incorporating human actions into PRA studies. The term SHARP stands for Systematic Human Action Reliability Procedures. SHARP is not intended to prescribe the method of human reliability analysis, but rather to stimulate analysts to state their assumptions explicitly. The framework developed is in the form of seven steps, each containing activities and rules and designed to be integrated with tasks typically performed in a PRA study. The seven steps are: definition, screening, breakdown (of influence factors), representation, impact assessment, quantification and documentation. The SHARP approach was given a small scale benchmark test on a standard problem

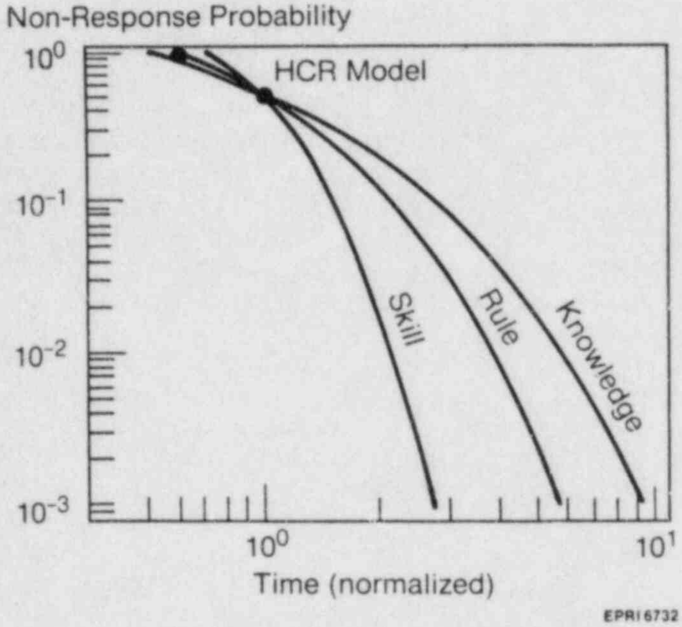


Figure 5. Human reliability model.

during 1984. The participants were NUS Corporation, Pickard, Lowe & Garrick and Electricite de France. A comparative analysis of the benchmark results is presented in a report currently in draft form. The IEEE SC7.2 subgroup on Human Reliability has completed a favorable review of the SHARP report and is in the process of issuing it as an IEEE guidance document.

Current work has the objective of preparing a usable model for representing operator reliability in a plant (16). This, of course, is particularly aimed at situations involving cognitive processing of information. Such a model must be reproducible, represent influence factors quantitatively, give probabilities as output and must include the cognitive process. In addition, it must be supportable by the existing database and be relatively simple to apply. Seventeen models were reviewed in the literature for features that could contribute (17). A model has been proposed (Figure 5) that consists of a parametrization of time-reliability curves that express probability of failure to respond correctly as a function of available response time (18). The parametrization is linked to Rasmussen's model, as previously discussed, of the control and decision process by a supporting Markov analysis, and is implemented interactively on a microcomputer. The preliminary version, in draft form for review, addresses skill, rule and knowledge-based actions, operator experience level, quality of operator/plant interface, and the stress level. A specific data input requirement is the median time needed for task completion.

This model will need extensive peer review and calibration against data before being implemented in systems analysis. However, the development of models of this type is an essential step to understanding the kind of data that must be obtained experimentally. A generalization of the HCR model to multiple phases of a sequence that incorporates correction of mistakes and the commission of slips, has been proposed as the next step in HCR model development. Such models will receive their first use in designing experiments. Later, it is anticipated that data collection using plant simulators will be initiated in an expansion of the current project. A review is underway (19) of the data most in need of experimental measurement, and of the facilities and techniques that will be needed to implement such experiments.

4.2 Diagnosis and Decision Making

The extensive use of simulators for training, control and diagnosis means that they should be qualified for the users. A general qualification methodology has been developed which emphasizes training requirements for simulator fidelity (20,21). The approach is directed towards satisfying in part the NRC requirements under Regulatory Guide 1.149.

The first part of the project has been successfully completed. The major achievement is the development of the systematic methodology to qualify training simulator software, and its capability to adequately simulate abnormal conditions in both PWR's and BWR's.

Several case studies were performed with this methodology. Its ability to uncover modeling shortcomings in some simulators was demonstrated (20).

The next phase involves extending this methodology to more comprehensively cover aspects of training simulator qualification, maintenance and

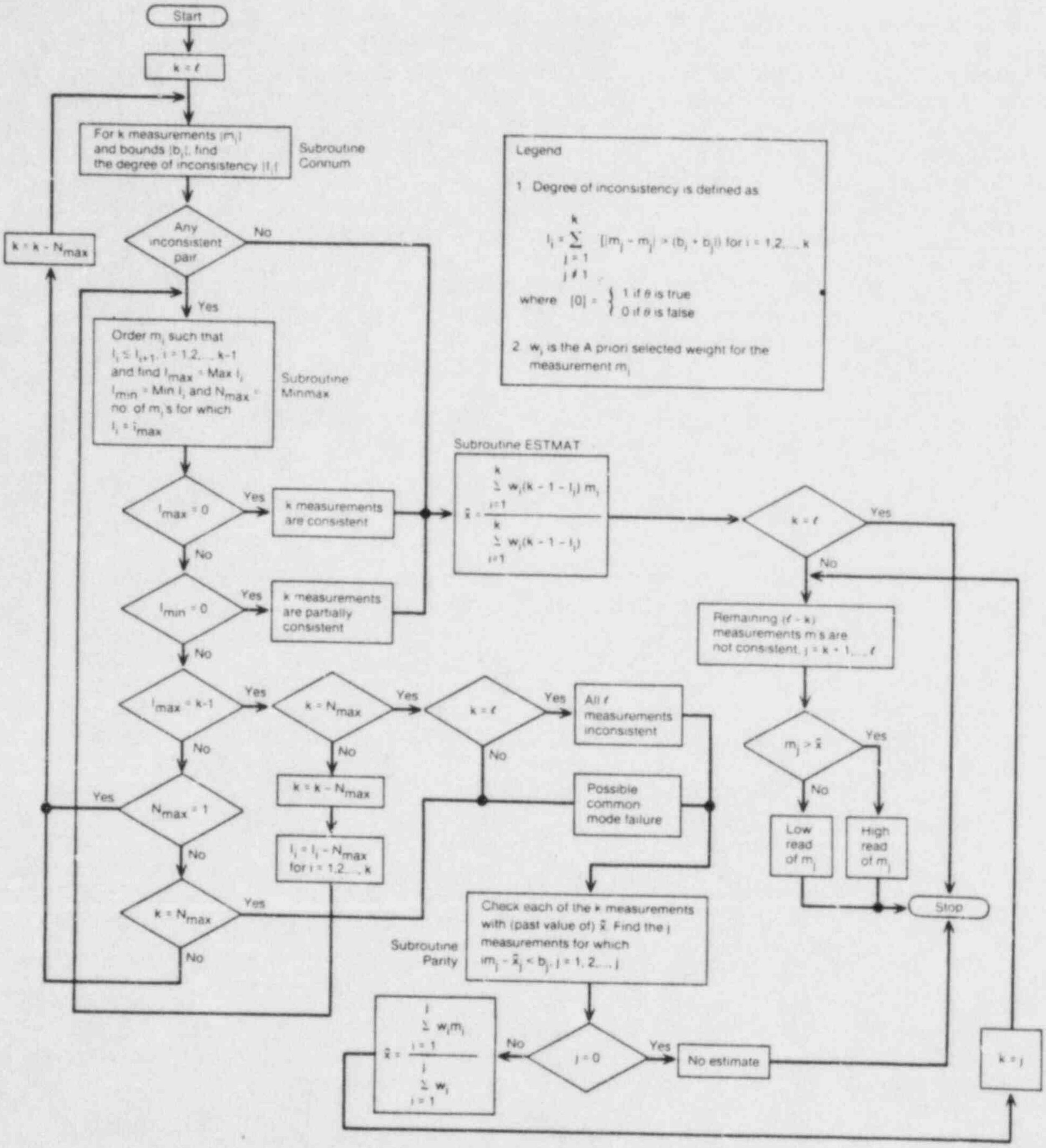


Figure 6a. Decision/Estimator Flow Chart.

configuration control, including compliance with impending NRC regulations. The project is conducted in cooperation with two utilities, which demonstrate the usage of the approach.

Means of improving the information available to the operator and the technical crew for diagnosis and decision making are also being developed. By processing the plant data and signals through man-machine interface systems, one can enhance the plant operator's ability in monitoring, diagnostics, and control in the event of plant transient and accident conditions. Near term efforts are to develop on-line real-time software for validation of safety parameter signals as pre-processors for plant safety parameter display systems (SPDS) (22), and for linking plant process computers and on-line core monitoring systems. The specifications for validation of SPDS signals have been completed (23). A typical decision estimator which is embedded in the software is shown in Figure 6a and the software design for plant implementation is shown in Figure 6b. Longer term efforts include the demonstration of software for core power monitoring and maneuvering, and post-scram analysis. Prototypes include two ideas:

- an explanation system involving expert system (rule-based) diagnosis of scrams

- a display hierarchy including the major functions required to confirm scram conditions.

A typical diagnostic system is shown in Figure 7, and these prototypes are currently under review by the industry (24,25).

### 4.3 Procedure Guideline Selection and Execution

Recent developments have occurred in the description and logic of emergency procedure guidelines, developing logic to track through the procedures, including the entry and exit conditions. It is possible to display this information in conjunction with critical plant parameters. Such software has been developed for the BWR (26) utilizing a graphics display building package called IMAGE, developed specifically for utility and industry use. The implementation and testing of such systems is now planned to determine the improvement in performance which results. The actual selection of procedures in transients can also be linked to the information model of the control process (Figure 8).

The areas that support procedure development and validation are those where phenomenological understanding is developed. These are related to the more traditional areas of reactor safety research, but with a markedly different emphasis.

The verification of analyses for B&W plants has lead to the joint NRC/EPRI/B&W OG/B&W project on integral system testing at B&W (27). The objective is to develop small break and natural circulation data which in turn enables the methods (codes) to be benchmarked which have been used to develop procedures.

First, the OTIS tests have been completed. Those tests simulate the events that would occur following an accident at a "raised loop" plant (e.g., Davis-Besse, Oconee). Data from the fifteen tests performed on OTIS are currently

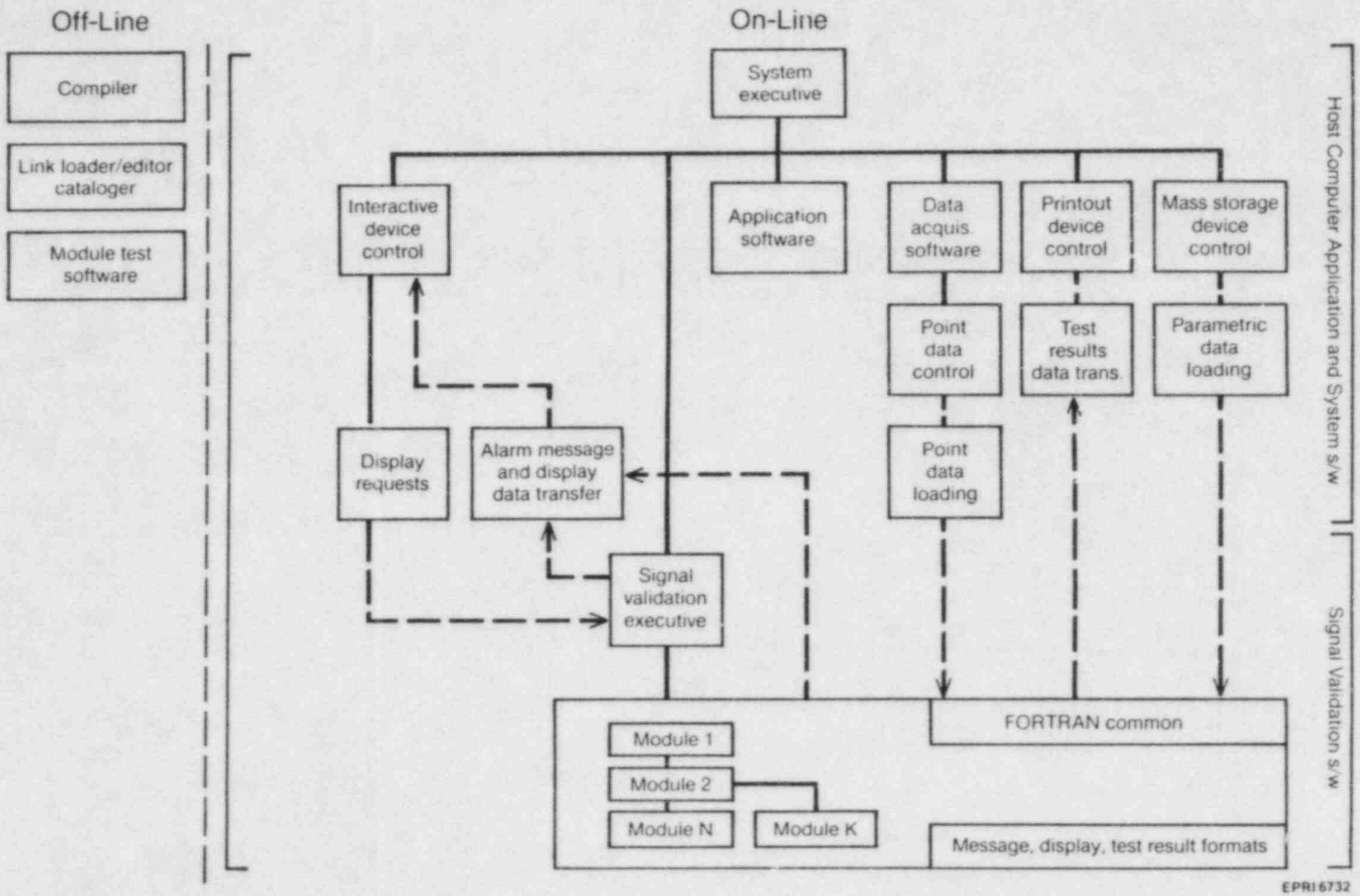


Figure 6b. Software block diagram, showing the interface between the host computer software and the signal validation software.

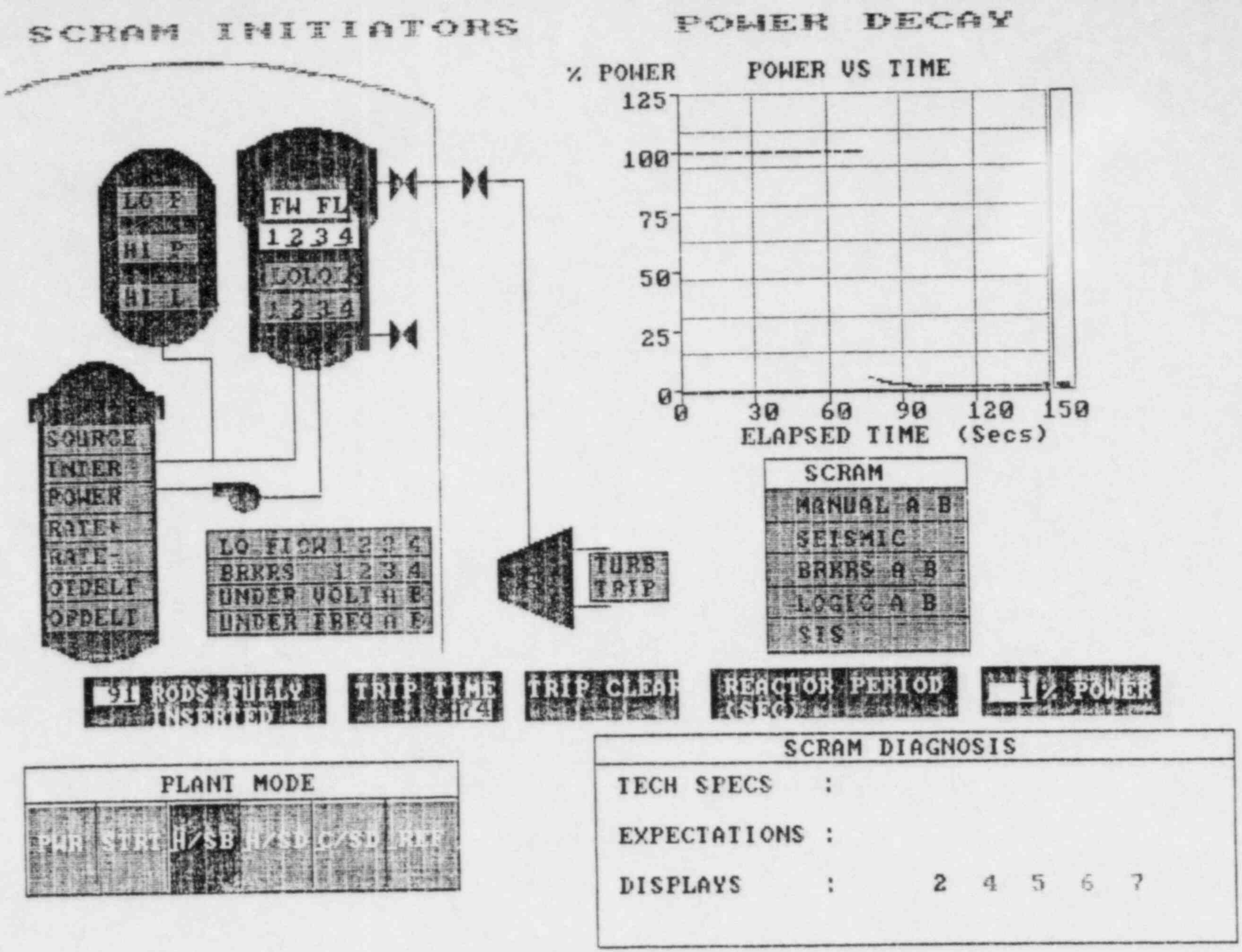


Figure 7. Display of SCRAM initiators and power decay in the PWR SCRAM analyzer.

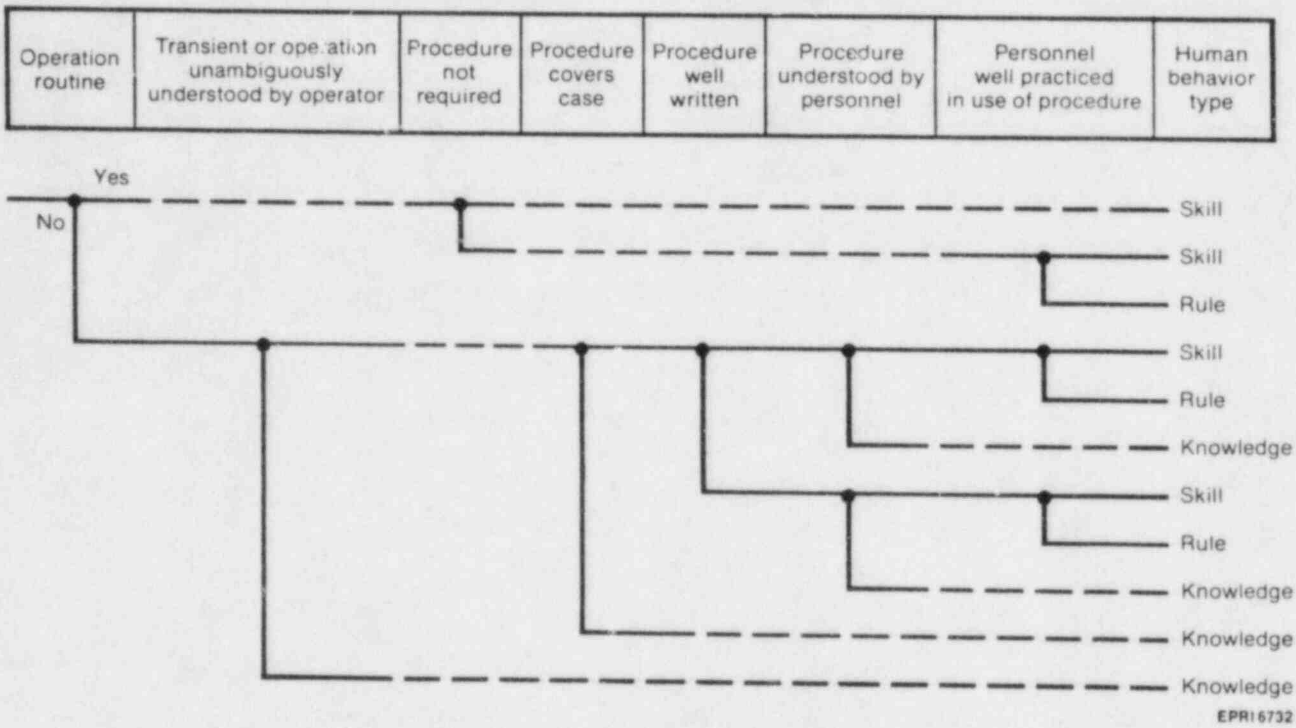


Figure 8. Logic tree to aid in selection of expected behavior type.

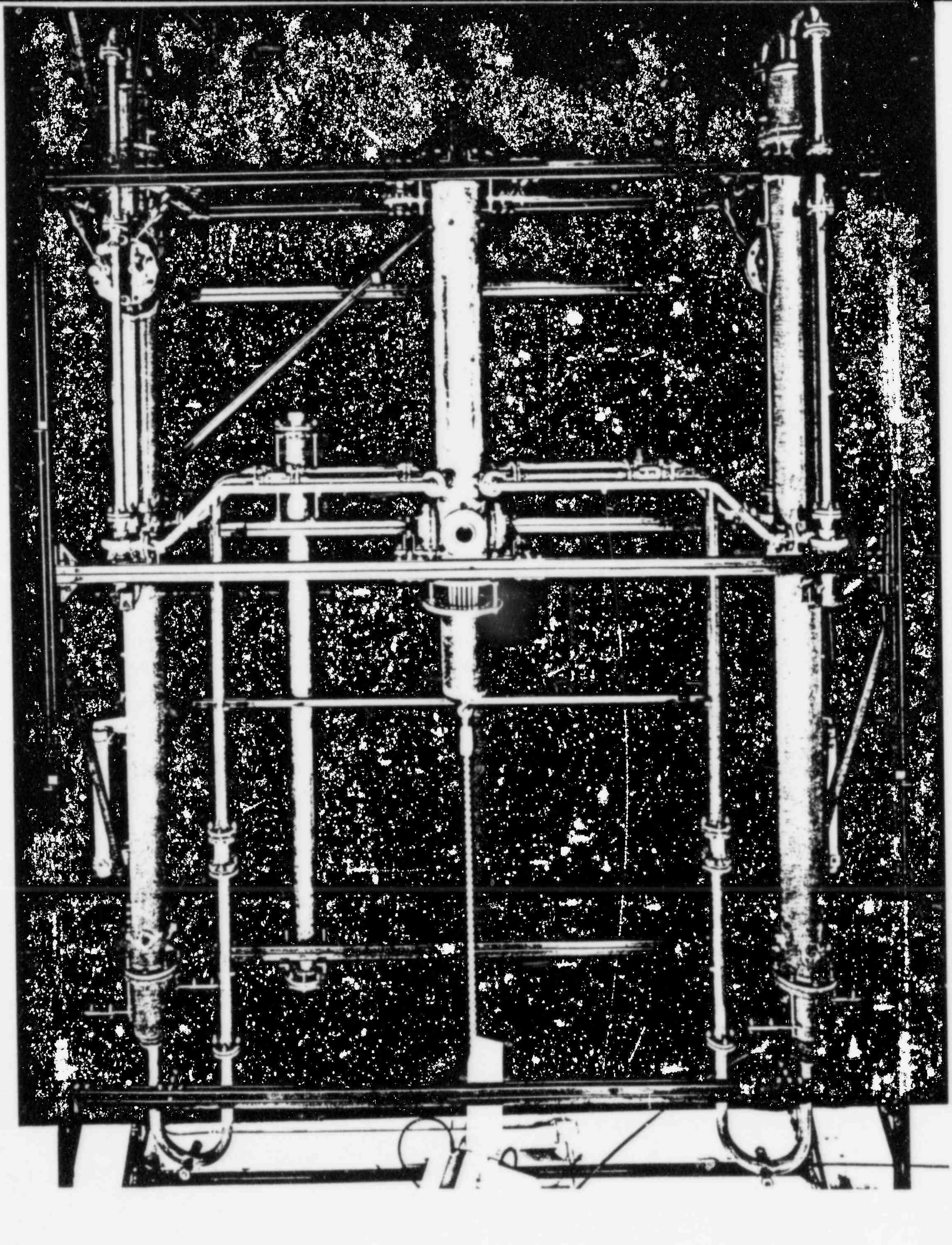

Figure 9.

being analyzed. The results to date support the B&W emergency operating procedures (28).

The MIST facility, representing the majority of U.S. B&W plant design (lower loop) has been designed and constructed. The loop construction has been successfully completed, with shakedown testing scheduled for the fall of 1985. There are important scaling questions which need to be addressed, and EPRI has undertaken a series of projects to support the OTIS/MIST program to ensure that, in conjunction with the NRC and utilities, major technical questions are capable of technical resolution.

To investigate the scaling rationale, an integral test facility was designed and constructed (Figure 9). The facility is scaled according to Ishii's new approach, and operates at relatively low pressure (100 psig). The major objectives of the facility are (1) to generate data for an alternate scaling rationale and (2) to complement the MIST facility for the study of specific phenomena (e.g., interruption of natural circulation in the "candy cane" and loop-to-loop oscillations). The scaling and design approach has been published (29).

To resolve the traditional, but thorny question of pipe diameter effects on scaling (from typical tests at 2", to 36" for the actual reactor size), flow regime facilities were designed to gain insight into the void distribution in the hot leg during typical transients. It is also important to determine the conditions for interruption of natural circulation. Two facilities were constructed at different scales in order to evaluate the diameter effects on flow regimes (Figure 10). The major achievement and result is the confirmation that the bubbly flow regime is predominant for large pipe diameters and the slug flow regime is suppressed (30).

A further question is the distribution of auxiliary feedwater, which wets some of the heat exchanger tubes and determines the thermal center for natural circulation. The operator can inject and throttle this flow. The auxiliary feedwater facility is constructed in order to simulate the physical processes in the secondary side of the B&W steam generator, as highly subcooled water is injected into superheated vapor. The scaling analysis has been completed. The results will be used in developing a phenomenological model at Dartmouth College, and will also be used for computer code modeling (31).

The two-phase flow characteristics of pumps is a problem for scaled facilities. It is now well known that the head degradation for those pumps used in small loops is very dissimilar to that for reactor pumps. Two years ago, EPRI initiated an analytical project at TETRATECH, aimed at developing a qualified first principles two-phase flow pump model. A major milestone was recently achieved (32) with the demonstration that the model is able to reproduce virtually all head/flow degradation curves available in the open literature (e.g., from Semiscale, LOFT, CREARE, B&W. CE). The model will now be used to simulate the MIST pumps and the prototype pumps, thus allowing the safety computer codes to extrapolate from scaled pumps to actual plant behavior. This has impact on the issue of whether to restart the pumps in a small break, and on the inventory loss due to pump operation.

It should be noted that these support projects, although sponsored by EPRI, are fully coordinated with other NRC-sponsored projects. The objective of

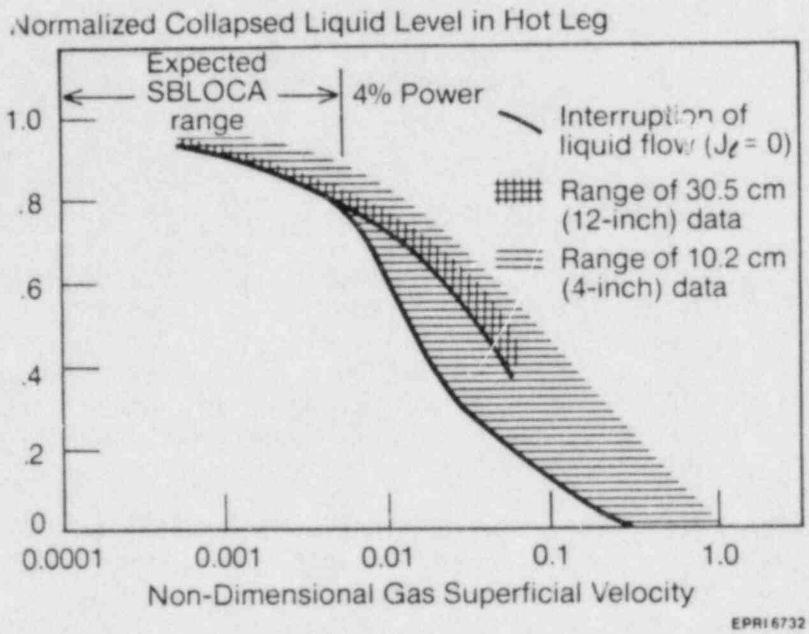


Figure 10a. Effect of pipe diameter on interruption of liquid flow over hot leg.

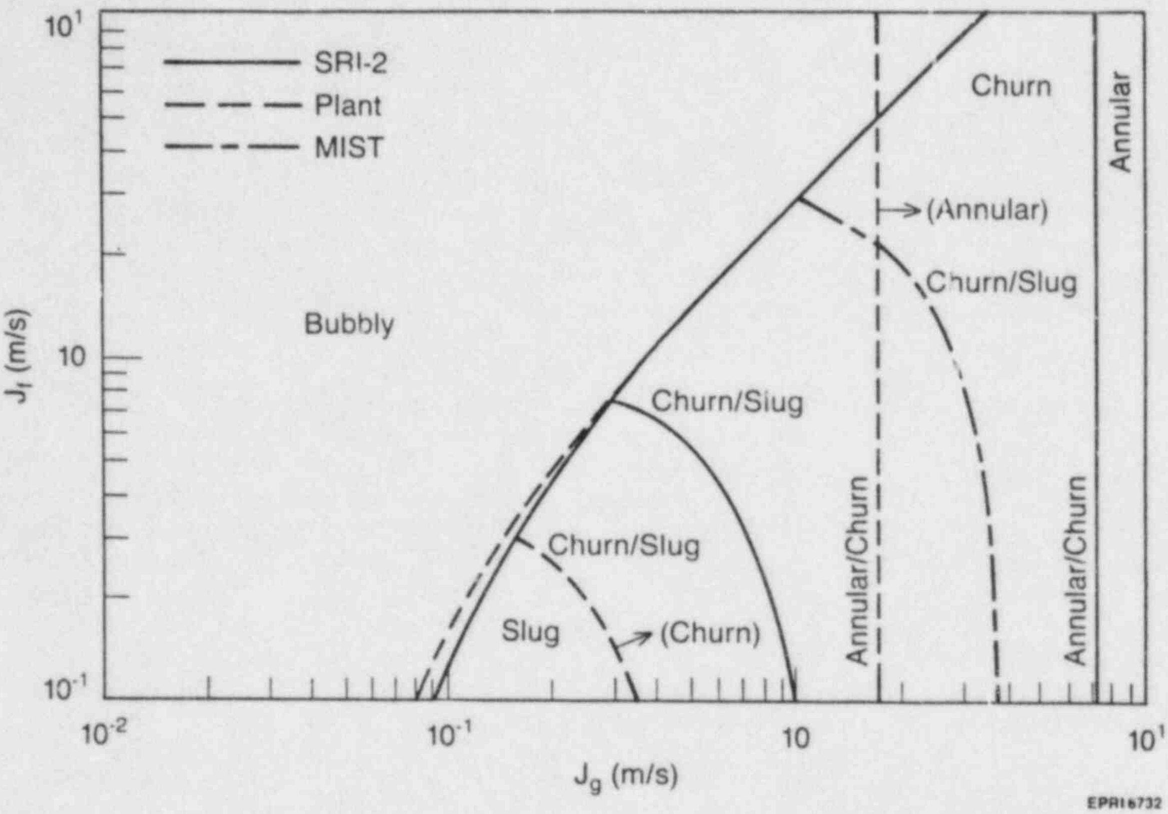


Figure 10b. Flow regime transitions.

this coordination is to cover experimentally and analytically all aspects of abnormal operating transients that a B&W plant can experience.

For BWR's, the joint EPRI/NRC/GE project to study abnormal transients in BWR's was successfully completed. The second testing campaign included nine tests aimed at the study of steam line breaks, feedline breaks and ATWS (33). In addition, a computer model, TRAC-BD was jointly developed which has proven very successful in reproducing the experimental data. It constitutes now the benchmark code for the industry, and has been used by GE to support new LOCA analyses (34,35).

## 5. Accident Sequences and Consequences: Plant Recovery

There are three means to ensure that given an accident the consequences are minimized in terms of damage and activity release. First, one can show that the sequence of events is understood, and that intervention is possible to reduce likelihood and severity; second, one can demonstrate that the system can accommodate the event; and third, the actual radioactive release can be shown to be acceptable, or lower than expected: the "source term" issue. More detailed information on this latter issue can be found in the companion paper to this meeting (36).

The quantification of recovery actions varied considerably among the PRA studies reviewed and is observed to have an extremely significant effect on frequencies of core damage sequences and on overall core damage frequency. The most complete and obvious measure of the influence of recovery is provided by the ANO study which displays an explicit, net recovery factor for each cutset of each accident sequence and the overall effect of recovery on core damage frequency: a reduction by a factor of 5.5 on core damage frequency. The other studies all include time-constrained recovery factors for several key systems that have significant effects: assumption of recoverability of feedwater reduces the core damage frequency by about half; the assumption of recoverability of certain failed pumps and valves in the auxiliary feedwater system for Zion reduces the system unavailability by about an order of magnitude and reduces the core damage frequency for internal events by a factor of about 2.6.

The use of the RETRAN systems analysis code has now extended into areas relevant to accident prevention and control, namely, procedure evaluation (1) setpoints and simulator qualification (21) In Figure 11, we show a typical utility usage of the code in the review of plant information obtained during a planned startup transient.

The evaluation of accident and transient sequences rests on an adequate estimation of reactor trip and setpoints. This places an important incentive on enhancing and improving the accuracy of the calculational methods used for setpoint determinations. Because such setpoints have safety significance, establishing a traceable historical record of the computations is highly desirable. This is the purpose of the RASP effort (37), which includes an auditable trace capability through the many calculations.

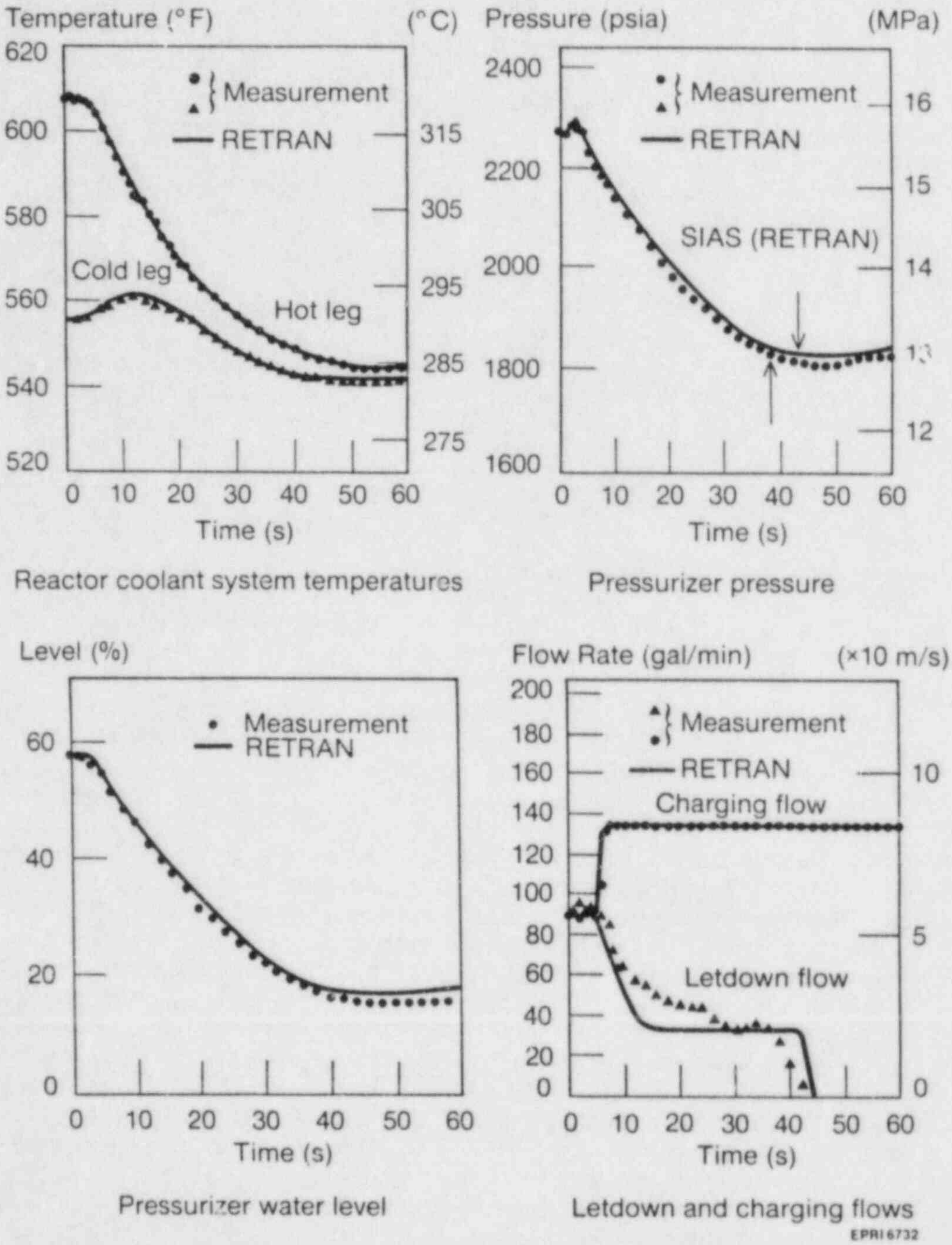


Figure 11. Turbine trip comparisons vs plant data (from Ref. 1).

## 5.1 Critical Components and Important Sequences

In this first area, PRA techniques show the accident sequences in detail. By reviewing these overall sequences, and the PRAs performed to date:

(1) the contribution of human error to the overall risk can be identified;

(2) the overall major sequences are understood in terms of the significant risk contributors and physical processes.

Contributing factors to the sequence assessments are 1) the site-specific frequency of loss of off-site power (LOSP); 2) the reliability of the plant-specific emergency power system, including potential inter-ties with a second unit on the same site and the influence of test and maintenance procedure to maintain the power systems; and 3) the ability of the specific plant designs to maintain some form of core cooling for limited times without AC or DC power being available.

### 5.1.1 Predicted Operator Actions

In addition to the variability observed because of different quantification pathways used in the studies, the review also shows that the relative importance of a given action varies between studies as influenced by the type of reactor and plant-specific design and operations. For example:

- in the Limerick PRA, as for many other BWRs, the principal predicted operator action contributing to core damage frequency occurs in transient sequences; and does not initiate reactor depressurization (ADS) which appears in several dominant core damage sequences.

- for Zion and ANO, which are PWRs, the most significant predicted operator actions occur in LOCA sequences. In the Zion study, as in the PRAs for several PWRs, the dominant interaction in core damage sequences is the switchover to recirculation after either high -or low- pressure injection. Although to establishing recirculation is also seen to be a dominant contributor in small- and medium-LOCA sequences in the ANO study, the most significant is the operator not initiating high-pressure injection in the most dominant LOCA sequence. This result, which contrasts significantly with those of Zion, stems from the assumption that automatic injection will not result because in a very small LOCA the break size is too small for the sensors to detect.

### 5.2.1. Steam Generator Tube Rupture

With the increased attention given to operational emergencies, the steam generator has also received attention. The NRC reviewed the questions of activity release and operator actions for both single and multiple tube failures. A joint effort has been completed with NRC, Westinghouse, and CEGB, in which the activity release, deposition and transport, have been characterized up to the case of a dried-out generator. These tests were preceded by transients which examined the response of 5 MW model steam generator to loss-of-feedwater and steam-line break events (38).

The intent is to characterize the retention factors within the generator, the efficiency of the separators under transient conditions, and the carryover to the steam line. Special instrumentation and tracer techniques were used for this purpose. These unique tests are supplemented by development of analyses to predict the transport and retention by modeling the physical processes along with the release pathway.

### 5.2.2 Accident Evaluation: Effects of Thermal Convection

Consequences of the postulated PWR high pressure scenarios e.g., TMLB' and $S_2D$, (small LOCA with failure of ECC injection system), have been evaluated using mainly the "once-through" forced convection flow modeling, in which the steam and hydrogen generated, flow through the core, PWR vessel, piping and components, in a uni-directional fashion.

The "once-through" convective ows may not be valid during the core heat-up and degradation phase, when t ere is a radial variation of the steam heat-up and hydrogen generation in the core. Buoyancy-driven natural convective flows develop and distribute the heat generated in the core to the upper parts of the PWR vessel and the primary coolant system (PCS). These flows, therefore, affect (a) the magnitude and the rate of hydrogen generation, (b) the elapsed time before start of core melting, (c) the transport of fission products in the PWR PCS; (d) the temperature of the PCS piping and components, (e) the revaporization of the fission products from the PCS surfaces, and, lastly, and of particular importance, (f) the course of high pressure accidents.

Inclusion of natural convection flow modeling, and determination of its effect on the parameters mentioned above, is the objective of the recent analytical work with the CORMLT code (39,40,41) and of the recent experimental work at Westinghouse. Of particular significance is the temperature history of the PWR PCS to assess if, for the risk-dominant high pressure scenarios, a local failure in the PCS may occur before the postulated vessel melt-through and radically alter the consequences of such accidents.

Some results for the temperature are histories of PCS piping and pressurizer gas have been obtained from the CORMLT code. A representative result shown in Figure 12, it is seen that the temperatures of surge-line piping reach very high values before the onset of fuel melting in the core. At such temperatures, mechanical survivability is of question, when system pressures are ≈2400 psi.

The work at Westinghouse R&D Laboratories involves simulated accident conditions in a 1/7th linear-scale-replica of a Westinghouse PWR core, upper plenum and piping and steam generators. Experiments using low pressure water and $SF_6$ gas have been performed. Visual observations of flow patterns, motion picture records and velocity data were obtained.

Argonne National Laboratory uses the COMMIX-1A code (42), a three-dimensional fluid flow code, to analyze and pre-predict the data measured in the Westinghouse tests. The ultimate objective is to obtain benchmark predictions of the temperature histories in a few prototypic postulated accidents, and to use the results to normalize the predictions made with the CORMLT code for the relevant part of the in-vessel accident progression.

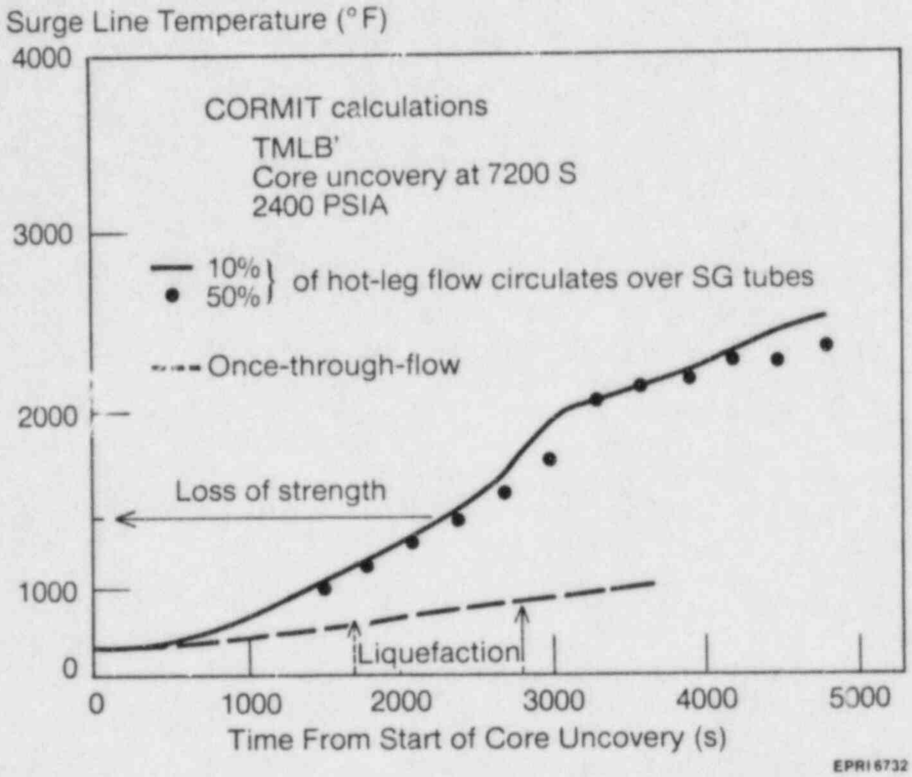


Figure 12. Effects of PCS flow options on thermal threats to PCS boundary.

Currently, there are ample data to show that natural convection flows play a significant role in the early part of the postulated high pressure severe accidents in PWRs. A substantial part of the heat generated in the core will be transferred to the primary system during the early part of these accidents. Initiation of melting in the core may be delayed substantially to provide the operator with more time to correct the faulted conditions. It is likely that local failures in the primary circuit to the pressurizer can be predicted. If verified, this will alter the course of a high pressure accident and will tend to reduce the containment challenge from direct heating loads.

### 5.2.3. Source Term and Consequence Analysis

The radiological source term for the Reactor Safety Study (RSS) PWR was updated using recently developed analytical methods and current experimental data (43). The analytical methods consisted mainly of the computer codes MARCH-23, PSTAC, CORCON, TRAP-MELT and MATADOR. Results of recent experimental programs were used to specify source material release rates and to define accident dependent aerosol retention factors for the analysis. Detailed plant data for the calculations were provided by Virginia Power Company, owner of the Surry Nuclear Power Station.

The accident sequences analyzed were determined from a re-examination of the RSS classification of dominant accident sequences for the Surry PWR (WASH-1400). This exercise eliminated $S_2C$ (small LOCA with containment injection spray failure) as a key accident sequence and identified TMLB' (transient with failure to recover electric power) and V (interfacing LOCA) as the risk dominant sequences requiring reanalysis. Thermal hydraulic calculations for the TMLB' sequence revealed it would likely be a "contained" accident instead of the early containment rupture sequence postulated in WASH-1400. Other recent work, which treats natural convection phenomena, also indicated that early venting of the PCS would occur in the sequence (44). This would reduce threats to containment integrity that would be associated with RPV breach at high pressures. Fission product transport analyses for this case produced small releases because of nominal containment leakage and efficient deposition processes. Prolonged containment integrity and/or additional fission product removal was expected to preserve the low releases. The resulting source term for TMLB' was more than a factor of 100 below the WASH-1400 estimate.

The V sequence was predicted to be the risk controlling accident. Its frequency was calculated to be a factor of 8 less than in WASH-1400 on the basis of testing procedures which had been introduced since completion of the RSS. In addition, deposition of fission product aerosols on surfaces along the transport path to the safeguards building, and capture by the water pool or by surfaces in the safeguards building, were calculated to result in a source term which was a factor of 10 less than the corresponding WASH-1400 assignment for the sequence.

One aspect of source term consideration is the integrity of reactor containment. In the accommodation of extreme accidents, the key question to be answered is whether the containment will fail given pressure and temperature histories associated with hypothetical, low probability, degraded core accident scenarios. And if so, what is the ultimate failure mode. This has significant ramifications, if the failure mode were a sudden gross one, such as a sudden rupture of containment wall, the fission product would be released to the environment immediately. However, if the failure mode were some localized leakage which would lead to containment depressurization, the release of fission product would be gradual and limited. Therefore to examine this capability, a research program has been undertaken by EPRI at Construction Technologies Laboratories (CTL) and Anatech International, Inc. The former has the testing responsibility, and the latter has the scope of developing an analytical model for failure mode prediction. Reference (1) summarizes the efforts to date with emphasis on experimental findings (45).

The public risk represented by the updated source terms was assessed with an improved version of the computer code (CRAC) that was used in the RSS. The site population and meteorological data used in the calculations also came from RSS files, as did the assumptions and rates regarding evacuation and relocation. The resulting analysis predicted no early fatalities for the updated source terms, and the latent fatality (cancer) risk was calculated to be about a factor of 20 lower than that produced by the corresponding RSS source terms.

The analytical methodology used to generate the new results was found to be clearly superior to the techniques used in the earlier RSS and this permitted a more realistic analysis of accident processes and fission product behavior. However, the methodology was cumbersome and expensive to apply because of the number of manual interfaces between computer codes, the various modeling or programming errors that required correction and the overall problem execution times that were encountered. Furthermore, instances arose where the methodology contained either inadequate or no models of phenomena/processes that were considered important to the source term calculations. In these cases the results of external analytical and experimental programs were utilized to provide alternate models or to guide simplifying assumptions. Limited sensitivity calculations were also performed to evaluate the impact of key parameters in these results and were factored into the definition of the best-estimate (updated) source terms.

### 5.1.3 In-Reactor Tests of Fission Product Evolution

The source term experimental program (STEP) is a series of in-reactor experiments designed to obtain data on fuel rod and fission product behavior under conditions representative of LWR severe accident sequences (46). The in-pile test unit, which is inserted into the TREAT reactor, comprises a bundle of four irradiated (burnup of 30,000 to 40,000 MWd/t) fuel rods mounted inside zirconia flow and insulator tubes (fuel rod temperatures in excess of 2033K (3200F) are expected) and contained in a high-temperature alloy pressure tube. Steam generated externally enters the bottom of the pressure tube and exits at the top, after having traversed the test fuel and a sample collection region. The sample collected region contains a series of deposition coupons at three different elevations, sequentially operated aerosol characterization sampling canisters, a hydrogen gas monitor and metal filters. Thermocouples are provided throughout the unit (47).

Four planned tests have been completed (48). The first test was a simulation of a PWR unterminated large-break LOCA (WASH-1400: AD), The second test was a simulation of a BWR feedwater failure transient with the concomitant failure of core makeup systems and residual heat removal systems (WASH-1400: TQUW), The third test simulated the loss of all AC power and of reactor coolant system heat removal in a PWR (WASH-1400: TMLB'), and the fourth was a TMLB' simulation with silver alloy control rods in place. These last two tests were being run under similar conditions.

Hydrogen and fission products were released in all tests. The hydrogen yielded generally agree with predictions of the more popular zirconium-steam reaction models. The relative fission product activity measured in the sample collection region of the in-pile test units varied between tests, being similar for STEP-1 and STEP-2, but were lower by at least an order of magnitude for STEP-3 and STEP-4. Axial temperature profiles measured during the latter two experiments indicate the possibility that natural circulation of gases occurred in the hotter regions.

Preliminary observations obtained from limited SEM, electron microprobe, and particle characterization analyses, have been done on STEP-1 samples Considerable amounts of tin were identified on deposition samples, and several samples sites indicated co-deposition of cesium with molybdenum, and cesium with iodine. In some cases, silver has also been identified as a component with cesium and iodine. The results of limited particle counting analyses indicate that for STEP-1, a relatively large population of submicrometer particles was formed, comprising the majority of particles that entered the aerosol canisters. Work is continuing on the post-test examination of data and samples from all four experiments.

Reactor testing has also been undertaken in conjunction with the LOFT consortium, sponsored by the U.S., DOE, EPRI and governmental agencies from eight other nations.

The testing program was concluded in 1985 with two significant tests: FP-1 and FP-2, both dedicated to the study of fission product transport in the upper plenum, upper head and other regions of the primary system.

In particular, the FP-2 test called for the actual meltdown of a fuel bundle in a configuration particularly chosen for simulation of a V sequence. The test was successfully performed in July 1985. The fission product concentrations at different locations are now being analyzed.

6. Coordination with Utilities and Government

In order for EPRI, which is a R&D arm of the electric utility industry, to succeed in developing computerized systems for demonstration and implementation in the nuclear power plants, the involvement and coordination with utilities is a key factor in the overall development process. In the early stages of the project, the utilities work jointly with EPRI to define the workscope and directions. In the technology development stage, the utilities serve as "participants" or "reviewers", and near the final stage the utilities serve as "users" or "hosts" in testing and implementing the technology.

The development of adequate tools for monitoring, diagnostics and control for plant safety and operation requires particular interaction with the utility industry to gain broad operator acceptance for plant implementation. The deployment of new computer systems to existing power plants requires careful planning, coordination and integration to minimize the effect of plant retrofit.

The Safety Control Systems and related projects are very closely coordinated with utilities throughout the technology transfer effort. These include a technology demonstration with host utility plants and project direction and input from interested utility advisory groups. Specific examples are: PWR power shape monitoring (PSE&G), BWR advanced core monitoring framework (TVA, CECO, etc.), safety parameter monitoring (BWR owners group).

Safety testing programs are closely coordinated with government and international efforts, not onlv to stretch scarce resources but to avoid unnecessary duplication and make the best use of available facilities. Examples of this endeavor in which EPRI is a major participant and some of which are discussed at this conference include the Marviken fission product tests, the LACE (HEDL) aerosol experiments, the TREAT (DOE/ANL) reactor fuel damage series, the MB-2 (W) SGTR evaluation, and the LOFT (INEL) international reactor safety tests. We continue to support such efforts where technical issues can be addressed and resolved in an efficient, coordinated and timely manner.

## 7. Conclusions

The safety research we have described is aimed at supporting the continued economic operation of today's power plants. It is oriented towards the goal of increased understanding and awareness of the methods by which technology can enhance control and accommodation strategies. We believe this represents a true and appropriate reforming of EPRI safety research efforts.

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# RESOLUTION OF STEAM GENERATOR INTEGRITY QUESTIONS

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## Introduction

The principal safety question regarding steam generators in pressurized water reactors is the effect on public health and safety of steam generator tube rupture events. Most of the research sponsored by EPRI and the Steam Generator Owners Group addresses steam generator reliability and thus the detection, correction, and long term prevention of conditions that could lead to tube rupture. This work has shown that careful maintenance of secondary water chemistry minimizes tube attack in steam generators. Moreover, routine nondestructive examination of steam generator tubes with the eddy current technique can detect widespread attack, can monitor the progression of defects in tubes, and can detect many defects before they leak. Other forms of nondestructive examination, e.g., use of fiber optic scopes and small television cameras, can detect effectively some of the conditions that lead to tube wear (1). Other work is underway to define the conditions that lead to flow induced vibration of tubes or parts that contact tubes, the objective being the prevention or correction of tube fretting and wear in steam generators.

EPRI and Steam Generator Owners Group results in the reliability area decrease the probability of the rupture of a large number of tubes in a steam generator and thus narrow the problem. This paper discusses EPRI's work in the safety area to define and minimize the impact of the few steam generator tube ruptures that may occur. The principal topics are leak-before-break, effect on core cooling, and radiation release.

## Leak-Before-Break

Leak-before-break means that a flaw in a tube will produce a detectable primary-to-secondary leak before it grows to a critical size that could rupture. Once a primary-to-secondary steam generator leak is detected, plant operators can take corrective actions to avoid a tube rupture.

There is a large class of defect types for which leak-before-break is well understood. This class consists of deep, small volume defects like pits or local wastage (2). There is a smaller class of defects for which leak-before-break does not apply. This class consists of deep but very large volume defects such as large wear scars and wastage over a large area. These types of defects have caused most of the tube ruptures that have occurred in PWRs but are amenable to corrective actions and detection by nondestructive examination (3). EPRI's leak-before-break analysis has, therefore, focused on cracks which are difficult to prevent and difficult to detect and size by nondestructive examination.

Generalized methods have been developed to calculate leak-before-break margins for steam generator tubes (4). The calculations are done in two parts:

- Critical crack lengths and leak rates are calculated.
- Crack growth rates are calculated to determine margins.

Critical crack lengths (i.e., the length of a crack that could rupture), and primary-to-secondary leak rates are calculated with PICEP (5), a code developed by EPRI to perform such calculations for stainless steel pipes and Inconel alloy 600 steam generator tubes. PICEP calculates both leak rates and critical crack lengths for the appropriate tube material, tube size, crack geometry and loads. The critical length of circumferential and axial cracks is calculated assuming plastic collapse of the net section. Crack opening area is computed using elastic-plastic fracture mechanics. Leak flow rates are calculated based on EPRI's modified version of Henry's homogeneous nonequilibrium critical flow model. In addition, crack shape and the roughness and number of turns in the leak flow path can be varied to better model cracks.

The rates at which cracks grow are calculated considering the predominant mechanisms of crack propagation. Once-through steam generators used in B&W design plants are straight tube designs. Differences in the thermal expansion of the tubes and shell during some transients and modes of operation give rise to axial loads on the tubes. If the axial loads were dominant, circumferentially oriented cracks would be expected. In fact, the cracks that have occurred in once-through steam generators have been circumferential predominantly and have had a morphology suggesting fatigue as the dominant method of crack propagation (6).

Recirculating steam generators, the types used in Westinghouse and Combustion Engineering design plants, are U-tube design in which both inlet and outlet ends of the tubes are anchored to a single tubesheet. To minimize axial loads the tubes are not axially restrained. Temperature differences between the inlet and outlet legs give rise to bending moments in the U-bends, but the dominant tube loads are in the hoop direction due to internal tube pressure. Consistent with such loading, the predominant orientation of cracks in tubes of recirculating steam generators has been axial (7). The cracks have been attributed to stress corrosion.

Analyses were performed for two representative cases: a once-through steam generator and a recirculating steam generator. For these general cases, the dominant mechanisms of crack growth were applied to demonstrate the technique and to calculate trends that are generic.

Analyses for once-through steam generators calculated that circumferential crack in tubes under axial tension would tend to grow through the wall and leak before they grew around the circumference. The critical crack lengths calculated for tubes in a typical once-through steam generator are listed in Table 1. Leak rates as a functon of crack length are shown in Figure 1 and typical leak-before-break margins are shown in Figure 2.

Axial cracks in the U-bend were analyzed for recirculating steam generators. The critical crack lengths are listed for key locations in Table 2. Leak rates as a function of crack length are shown in Figure 3 and typical leak-before-break margins are shown in Figure 4.

In both cases, detectable primary-to-secondary leakage provides margin between leak detection and growth of a crack to a critical length.

Core Cooling

Steam generator tube ruptures that have occurred in the Prairie Island-1 and Ginna plants and steam generator tube rupture experiments at the SEMISCALE and Model Boiler (MB-2) facilities have provided data for verification of the sytems thermal and hydraulic codes RETRAN and RELAP (8, 9, 10, 11) and vendor codes used for licensing. These codes, in turn, have been applied to calculate the responses of plants to postulated steam generator tube rupture events.

Analyses were performed to evaluate the impact on core cooling from ruptures of tubes in one or both steam generators of a Babcock & Wilcox plant during a large break loss-of-coolant accident (LOCA) (12). The RELAP4/MOD7 computer code was used to calculate the peak fuel-cladding temperatures reached during the LOCA transient assuming varying numbers of ruptured tubes--up to 300--in each steam generator. Analyses show that satisfactory core cooling is maintained in a B&W design PWR under the condition of steam generator tube ruptures in conjunction with a design basis LOCA. Calculated peak clad temperature versus the number of ruptured tubes is shown in Figure 5. The maximum fuel cladding temperature was calculated to be 1605°F, well below the licensing limit of 2200°F. Moreover, the calculations revealed no strong relationship between the maximum fuel-cladding temperature and the number of tubes assumed to have been ruptured in each steam generator.

Other analyses assessed the system response of a lower loop Babcock & Wilcox plant to the rupture of single or multiple tubes in one or both once-through steam generators (13). Calculations were performed with the RELAP5 computer code using the nodalization shown in Figure 6. The transient scenarios analyzed are outlined in Table 3. RELAP5 analyses were performed for the rupture of a single tube in OTSG-A, for the rupture of 10 tubes in OTSG-A, and for the simultaneous rupture of 5 tubes in OTSG-A and 5 tubes in OTSG-B. In addition, some analyses were repeated to assess the effect on plant response of continuing to operate reactor coolant pumps during the transient instead of tripping them.

Results of the analyses show that core cooling was maintained for all cases, i.e., no fuel rod cladding temperature excursions were calculated in any of the cases. In all cases, a controlled shutdown was achieved, with primary pressure below the lowest secondary safety valve opening set point and continuing to decrease and with hot leg subcooling margins greater than 20F . In cases where operation of reactor coolant pumps was continued during the transient following the steam generator tube rupture(s), both forced circulation and pressurizer spray were available. The plant recovery was calculated to be steadier and more easily controlled than in cases where the pumps were tripped early in the transient. In cases where the reactor coolant pumps were tripped, natural circulation and/or feed and bleed were sufficient to cool the plant as is illustrated by Figure 7 for the case of five ruptured tubes in each of the steam generators.

A Westinghouse design two loop plant has been modeled with RETRAN (14) and similar transient cases are being analyzed with similar results. Figure 8 shows the pressure response calculated for the rupture of a single tube with concurrent loss of offsite power. Experiments in the MB-2 facility discussed below have also provided thermal hydraulic data on other conditions beyond the design basis, conditions such as a stuck open relief valve in conjunction with a steam generator tube rupture.

## Radiation Release

EPRI work has proceeded on the parallel paths of using existing information to develop analytical techniques for calculating radiation release during a steam generator tube rupture event and determining experimentally the attenuation that occurs when radionuclides are transported through steam generators from primary-to-secondary leaks.

To address the radionuclide release from a tube rupture in a once-through steam generator, the basic processes of flashing at the leak and evaporation on heated tubes were modeled conservatively. Calculations for various tube rupture transients yielded average steam generator DFs several times greater than the values of 1 or 2 used in other analyses. Figure 9 is a plot of DF versus time for the rupture of a single tube in an OTSG, assuming constant steaming of the affected steam generator. The model is now being extended to recirculating steam generators.

The bulk of EPRI's work relative to radiation release has been experimental. Past experiments provided basic data on steam generator transient response to upset conditions (15-21). Current experiments build on this base. Some of the limitations of the small-scale experiments and fluid modeling experiments in producing prototypical behavior were overcome by running a selected test matrix in prototypical model boiler test facility in a cooperative effort with the NRC, Westinghouse, and, in part, CEGB.

The Model Boiler No. 2 (MB-2) test facility (22) is an approximately 1% power-scaled model of the Westinghouse Model F steam generator (Figure 10). It is designed to be geometrically and thermohydraulically similar to the prototype steam generator and is capable of generating a maximum of 10 MWth power. The MB-2 contains a 52-tube, 22-ft high tube bundle. The model boiler components are housed inside a 50-ft high, 32-in ID pressure vessel. With this test model, a series of transient tests modeling the following events has been performed:

- Loss of feedwater
- Steam line break
- Steam generator tube rupture
- Activity transport following steam generator tube rupture

A typical steam generator tube rupture experiment as shown in Figure 11 has been reported (23). Based on initial results, the experimental technique was refined and the series of tests shown in Table 4 were performed. Separate chemical tracers were used in the primary and secondary coolants of MB-2 to determine moisture carryover and the fraction originating from the primary side during a simulated double ended guillotine rupture of a tube. Data are being analyzed and will be used for validation of activity transport models. It appears that these data show steam generator partition factors one to several orders of magnitude higher than assumed in current design basis calculations, even for events beyond the design basis.

In addition, a tube rupture scenario following a severe degraded core accident was simulated. Aerosol retention tests were conducted to scope bounding conditions for evaluating steam generator aerosol attenuation factors due to loss of primary/secondary pressure boundary integrity. The tests were conduced at ambient temperature and pressure using water droplets of either 2 μm or 35 μm aerodynamic mass median diameter. These aerosols were introduced either at the top of the U-tubes or at the bottom of the tube bundle through injector tubes that simulated a tube rupture. The aerosols were sampled by milipore filters and cascade impactors at both the inlet to the injector and at the outlet of a stuck open relief valve located at the top center of the MB-2 shell. For an equivalent tube bundle velocity of 2 ft/s (representing a single tube rupture), the 35-μm drops were attenuated by a factor of 15 and the 2-μm droplets were attenuated by a factor of 3.3. This attenuation does not include any effects due to temperature and pressure, such as steam condensation on the walls of the secondary heat transfer system or enhancement of aerosol retention. These effects could result in an additional large attenuation (or retention) factor of an order of magnitude or more. Prototypical tests would be required to quantify these effects and provide realistic aerosol retention factors for severe accident scenarios.

## Conclusions

The principal safety question regarding steam generators is the effect on public health and safety of steam generator tube rupture events. Work being conducted by EPRI as well as others points to the following elements for resolving the questions:

a. Careful maintenance of secondary water chemistry minimizes tube attack in steam generators.

b. Routine non-destructive examination of steam generators can detect widespread tube attack, monitor the progression of attack in tubes and detect many mechanically and corrosion induced defects before they leak.

c. The majority of defects in steam generator tubes will leak detectably before they break. This leak-before-break concept covers cracks that may be difficult to detect by nondestructive inspection.

d. Even if multiple tubes were to rupture in steam generators, plant recovery can be handled using emergency procedures without loss of core cooling.

e. Analysis and experiments are yielding steam generator partition factors or DFs much larger than those assumed in design basis calculations. As a consequence, best estimate analyses are expected to show that even multiple steam generator tube ruptures can be handled without high radiation dose rates.

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Table 1

SUMMARY OF CRITICAL THROUGH-WALL CRACK LENGTHS
FOR OUTER TUBES IN A ONCE-THROUGH STEAM GENERATOR

| PLANT CONDITION | CATEGORY | LENGTH, $2a_c$ Inches | ANGLE $2\alpha$ |
|---|---|---|---|
| Cold Shutdown | Normal | 0.991 | 182° |
| Full Power | Normal | 1.130 | 207° |
| Cooldown | Normal | 0.774 | 142° |
| Feedwater Line Break | Emergency | -- | -- |
| Main Steam Line Break | Emergency | 0.376 | 69° |
| Reactor Trip | Upset | | |
| $\Delta T$ = 160°F | | 0.566 | 104° |
| $\Delta T$ = 85°F | | 0.871 | 160° |
| $\Delta T$ 50°F | | 1.060 | 195° |
| LOCA | Faulted | 0.485 | 89° |

Table 2

SUMMARY OF CRITICAL THROUGH-WALL CRACK LENGTHS IN INNER ROW U-BENDS IN A RECIRCULATING STEAM GENERATOR

| | | Critical Crack Length, Inches | | |
|---|---|---|---|---|
| | | Extrados | Flank | Intrados |
| Plant Condition | Location | $\phi = 0°$ | $\phi = 90°$ | $\phi = 180°$ |
| Steady-state | Cold leg ($\theta = 180°$) | 1.74 | 1.59 | 1.41 |
| | Apex ($\theta = 90°$) | 1.95 | 1.85 | 1.68 |
| Main steam line break | Cold leg ($\theta = 180°$) | 1.05 | 0.95 | 0.84 |
| | Apex ($\theta = 90°$) | 1.18 | 1.11 | 1.01 |

Table 3

ANALYSIS MATRIX

| Case No. | Number of Tubes | Pump Trip Criterion | Remarks |
|---|---|---|---|
| 1 | 1-OTSGA | At rupture | Benchmark analysis to compare RELAP5 with RETRAN and B&W MINITRAP analyses |
| 2 | 1-OTSGA | At scram | Natural circulation cooldown |
| 3 | 1-OTSGA | 20F° subcooling | Effect of delayed pump trip on baseline SGTR |
| 4 | 10-OTSGA | 20F° subcooling | Effect of larger number of ruptures |
| 5 | 5-OTSGA<br>5-OTSGB | 20°F subcooling | Effect of leaks in both steam generators |
| 6 | 10-OTSGA | 0F° subcooling | Effect of a less restrictive pum, trip criterion |

Table 4

PHASE II TEST MATRIX SUMMARY

| Test Type*** | | Primary* $P_1$ (psia), $T_1$ (°F) | Secondary* $P_2$ (psia) | Comments |
|---|---|---|---|---|
| 2.1 | Steady-state with SOR** bottom break | 170, 320 | 85 | To assess low velocity discharge without flashing. |
| 2.2 | Steady-state SORV bottom break | 557, 427 | 287 | To assess flashing break flow. |
| 2.3 | Steady-state with SORV bottom break | 1850, 580 | 1080 | To assess high velocity discharge without flashing. |
| 2.4 | Steady-state with SORV top break | 1850, 580 | 1080 | To assess effect of break location. |
| 2.5 | Steady-state primary top break no dryer | 1850, 560 | 1080 | To determine effect of dryer on carry-over. Upper shell must be removed. |
| 2.6 | Steady-state primary noncondensible carrier gas | - - | - | Aerosol retention following a severe accident and SGTR. Test conditions to be defined by EPRI. |
| 2.7 | Transient SGTR & SORV bottom break | Primary conditions taken from analytical calculations simulating PWR response to SGTR & SORV | | Auxiliary feed remains on until 15,000 s, then stopped stopped and SG allowed to boil dry. |
| 2.8 | Overfill transient top break | 1850, 580 | 1080 | Overfill simulated with normal safety valve operation, followed by SORV, no SGTR. |
| 2.9 | Transient SGTR & SORV top break | Same as test 2.7 | | Test 2.9 to be performed as a continuation of tests 2.4, with boundary conditions as close as possible to those for test 2.7. |

*Preliminary conditions - parameters to be confirmed following thermal-hydraulic support analysis.
**SORV - Stuck Open Safety Valve
***Tests are preceded by steady-state measurements without SGTR.

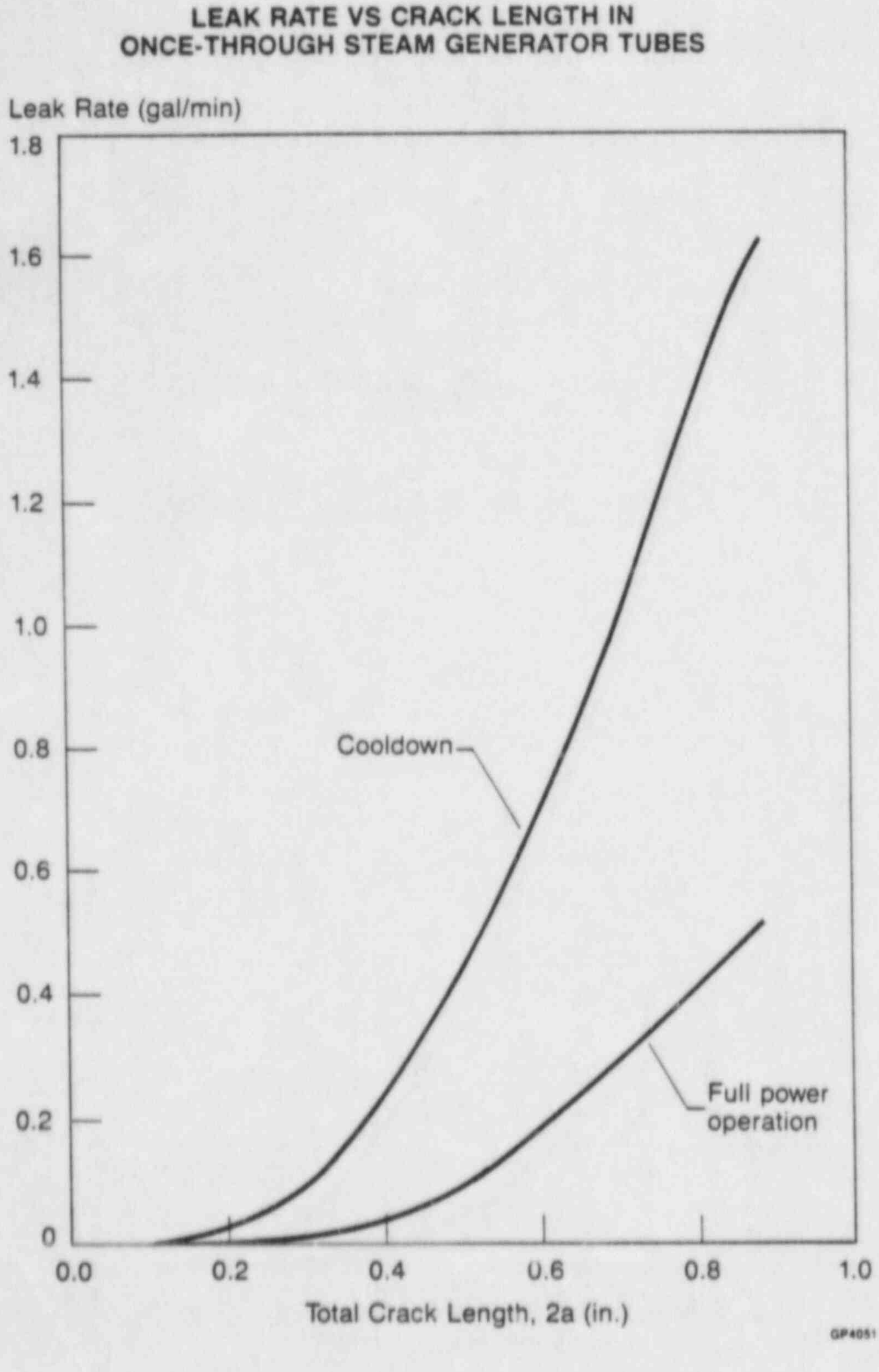
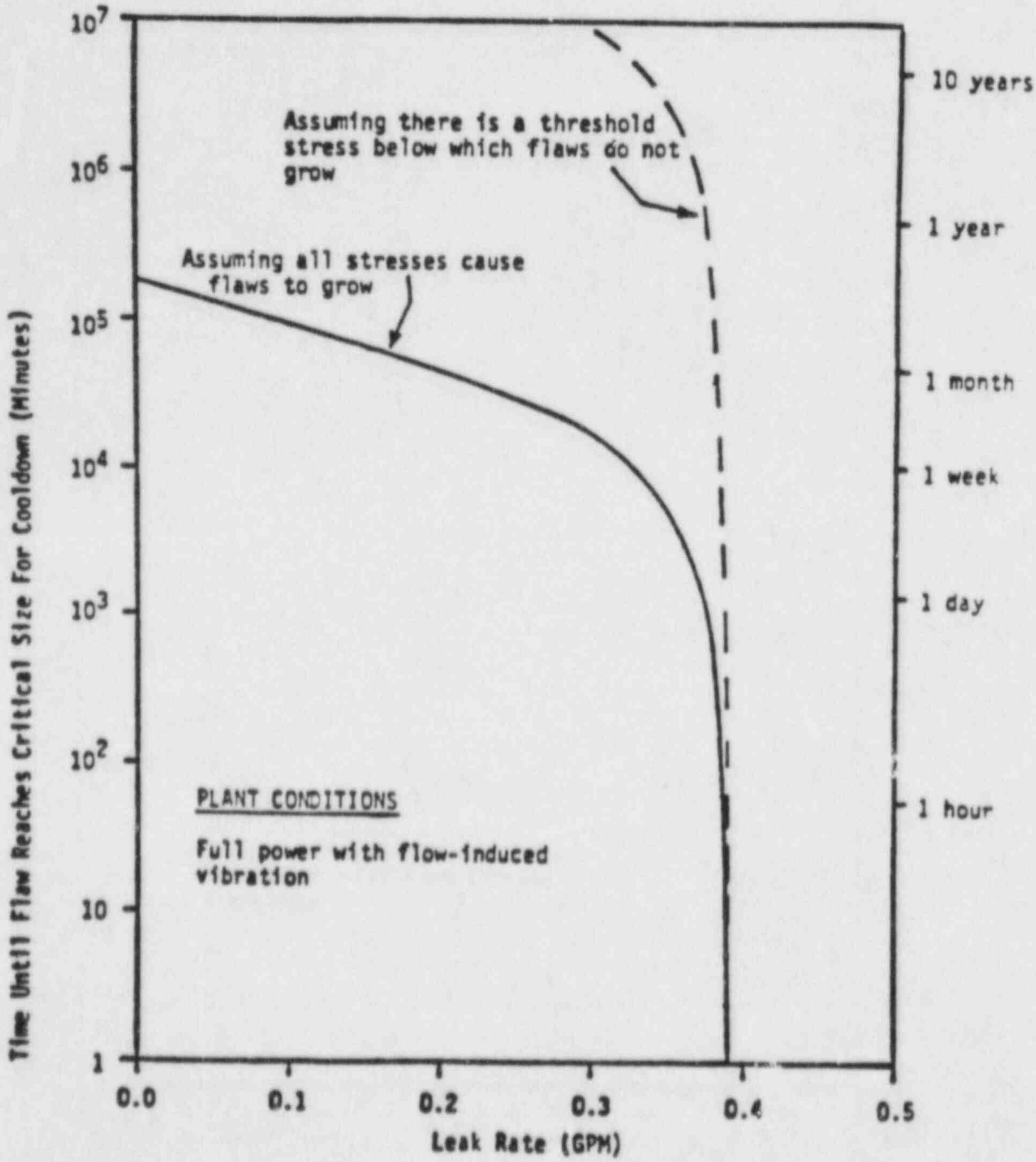

LEAK RATE VS CRACK LENGTH IN
ONCE-THROUGH STEAM GENERATOR TUBES
Leak Rate (gal/min)
1.8
1.6
1.4
1.2
1.0
0.8
0.6
0.4
0.2
0
0.0
0.2
0.4
0.6
0.8
1.0
Cooldown
Full power operation
Total Crack Length, 2a (in.)
GP4051

Figure 1

Time-To-Reach Critical Flaw Size for Cooldown by Fatigue Loading at Full Power Versus Detectable Leak Rate




Not Applicable if Stress Corrosion Cracking or Corrosion Assisted Fatigue Occur

Figure 2

## CALCULATED LEAK RATE VS CRACK LENGTH IN U-BEND REGION OF TUBE

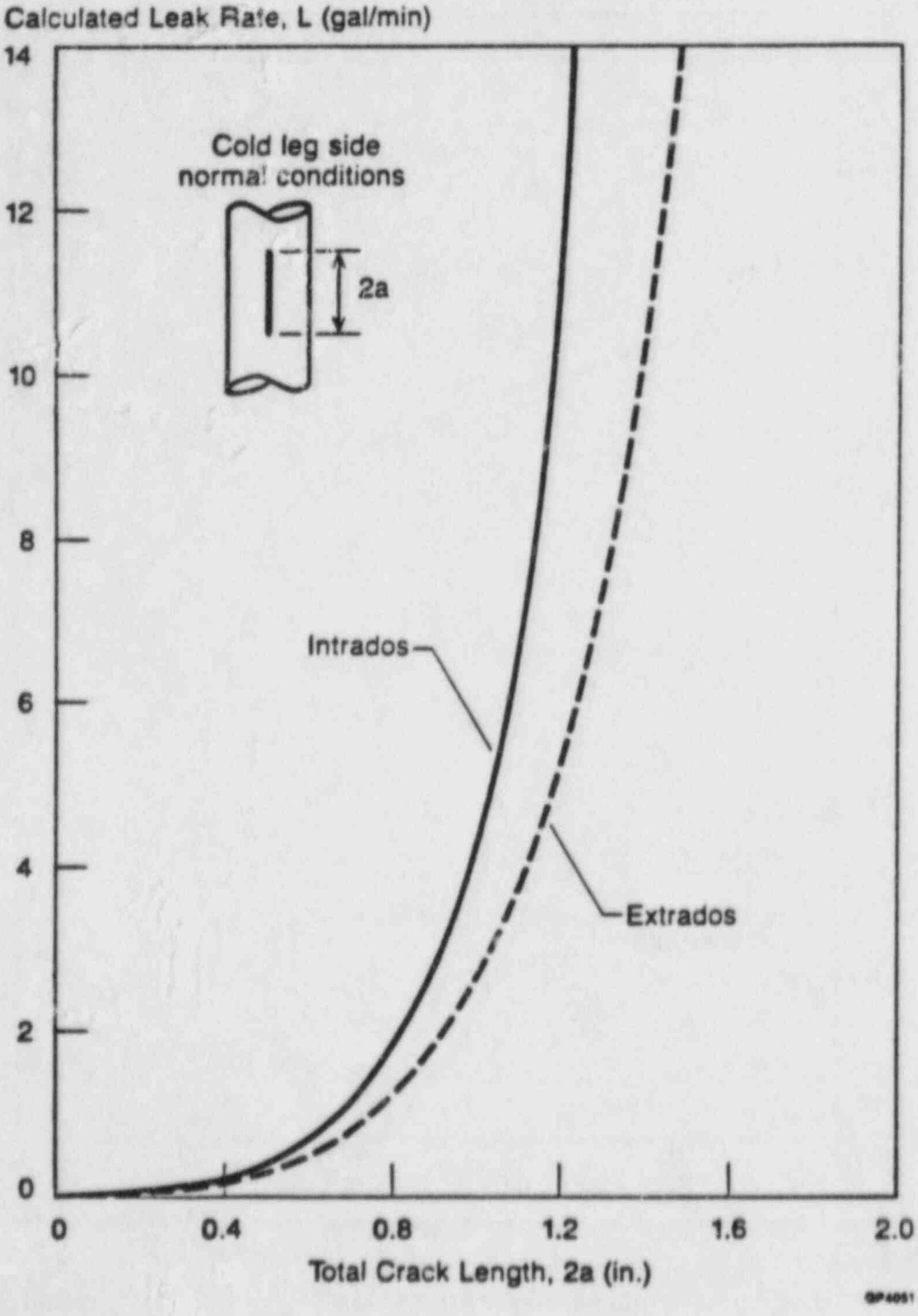



Figure 3

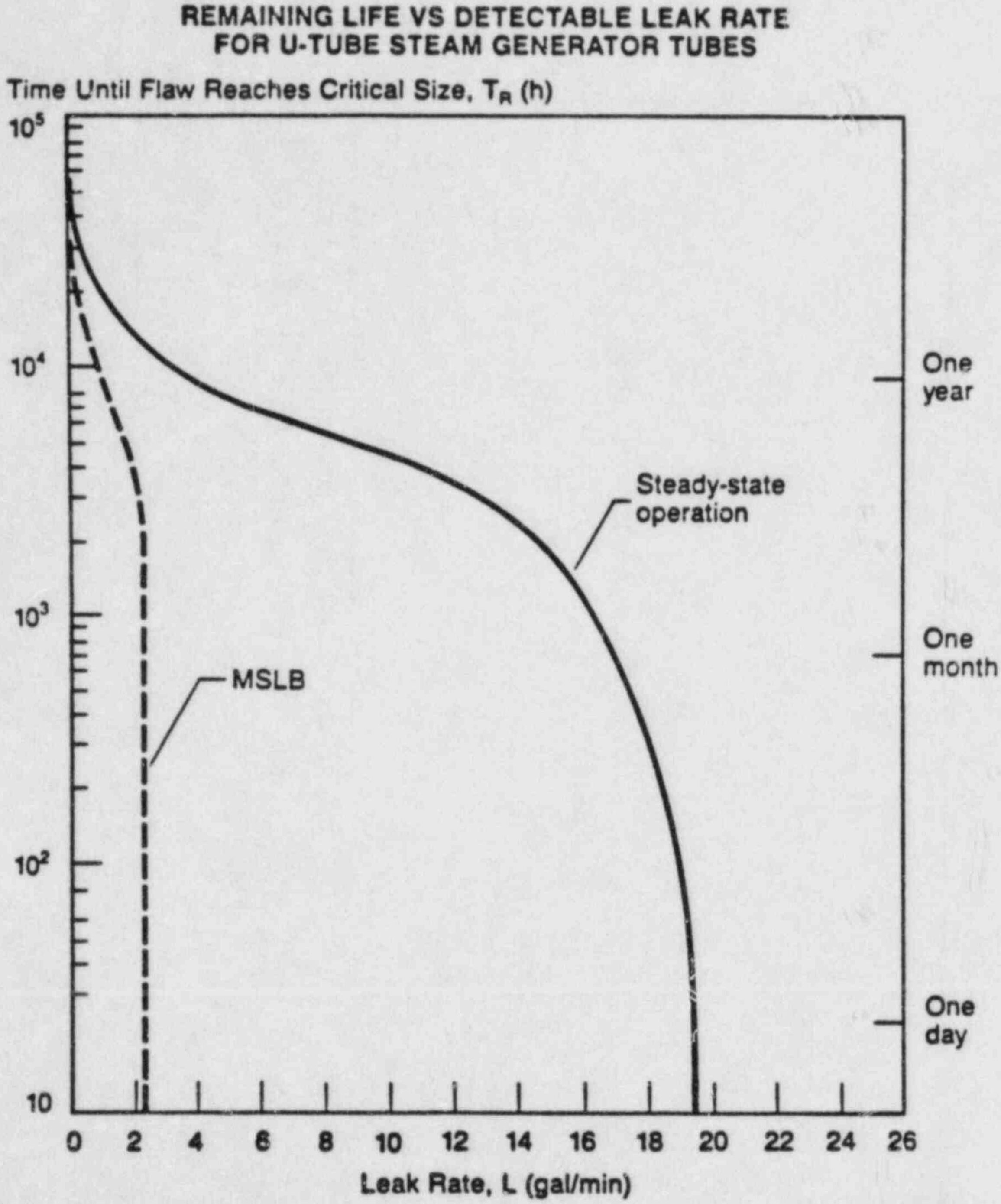

REMAINING LIFE VS DETECTABLE LEAK RATE
FOR U-TUBE STEAM GENERATOR TUBES
Time Until Flaw Reaches Critical Size, $T_R$ (h)
$10^5$
$10^4$
$10^3$
$10^2$
10
One year
One month
One day
Steady-state operation
MSLB
0
2
4
6
8
10
12
14
16
18
20
22
24
26
Leak Rate, L (gal/min)

Figure 4

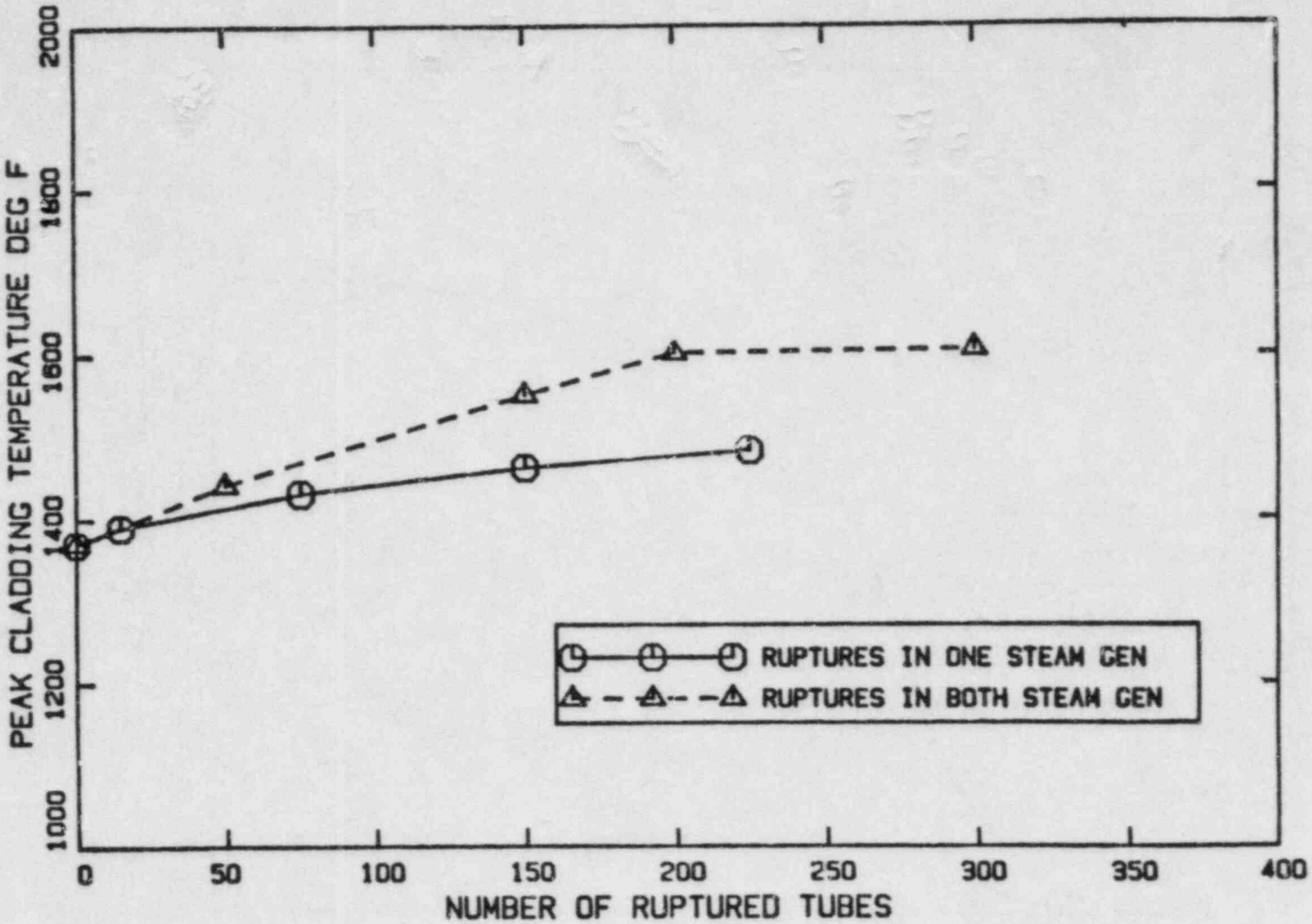

PEAK CLADDING TEMPERATURE DEG F
1000
1200
1400
1600
1800
2000
0
50
100
150
200
250
300
350
400
NUMBER OF RUPTURED TUBES
RUPTURES IN ONE STEAM GEN
RUPTURES IN BOTH STEAM GEN

Figure 5

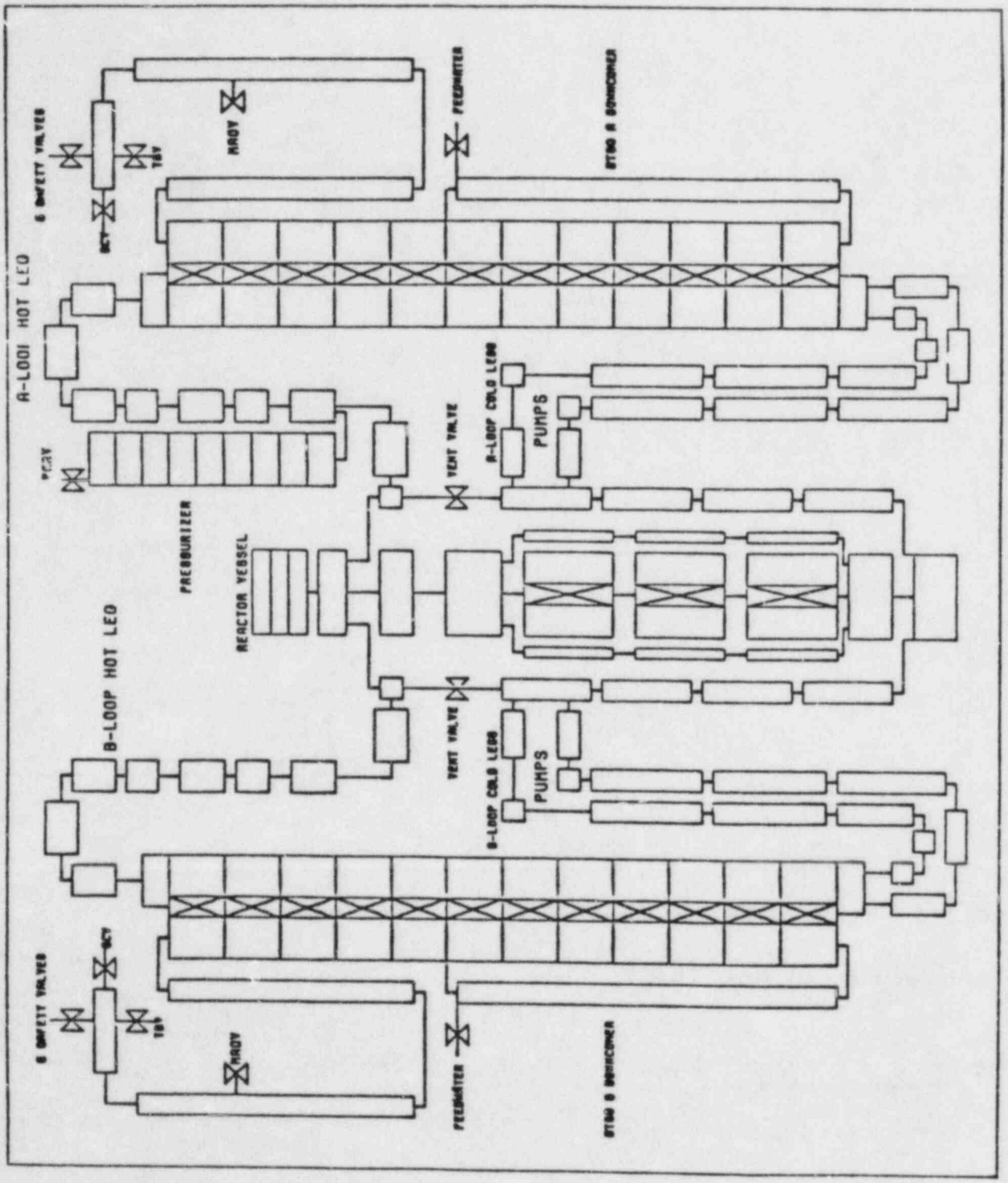
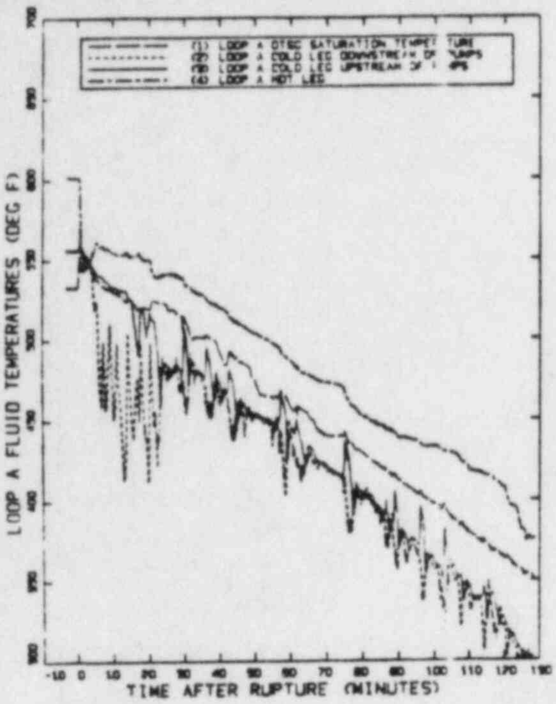
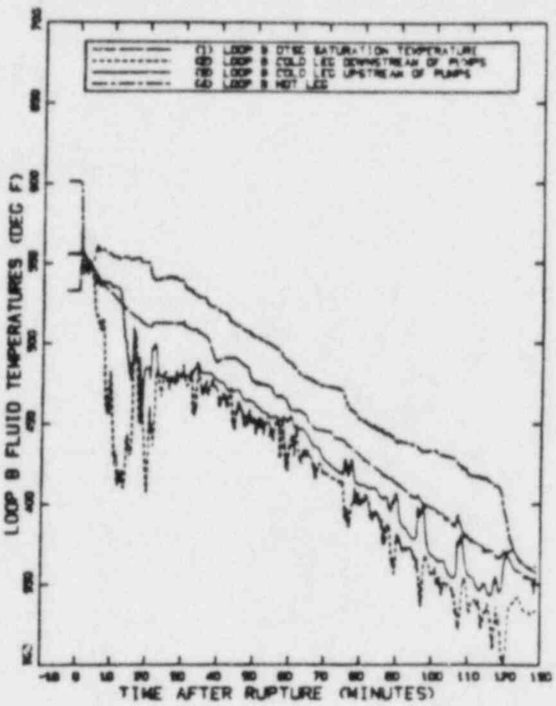


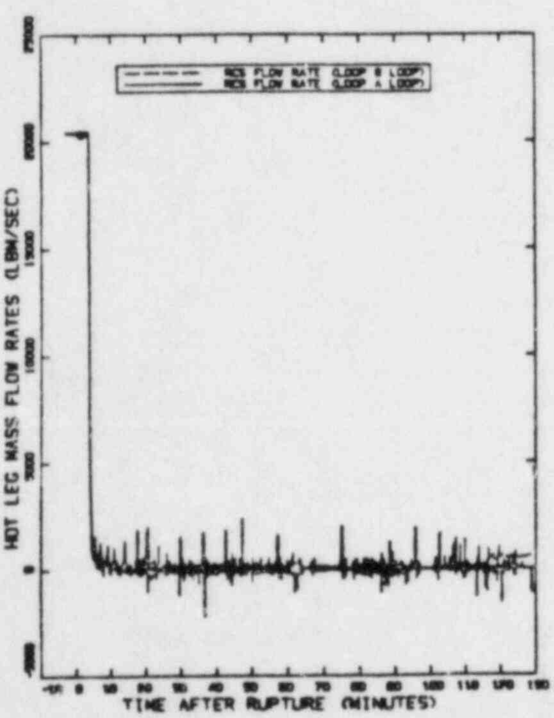

Figure 6. RELAP5 Nodalization Diagram



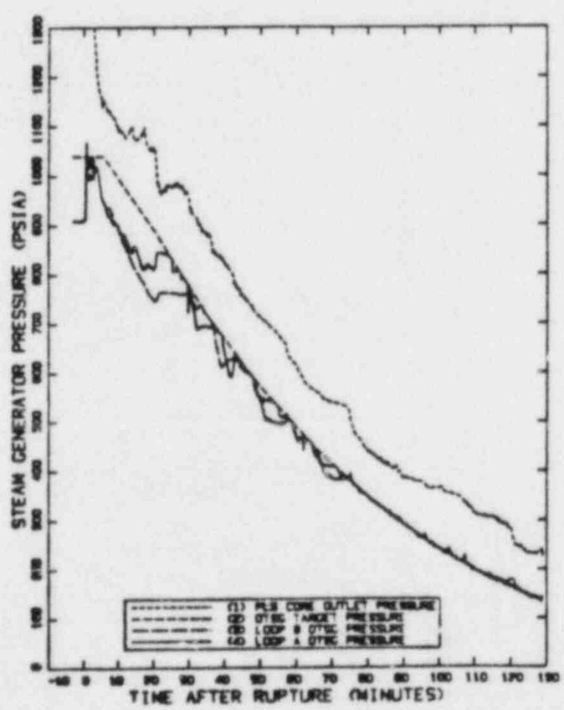

a. Loop A Fluid Temperatures




b. Loop B Fluid Temperatures




c. Loop A and B Hot Leg Mass Flow Rates




d. Steam Generator Pressures during Cooldown

Figure 7. Plant Cooldown Curves for the Rupture of Five Tubes in each Steam Generator.

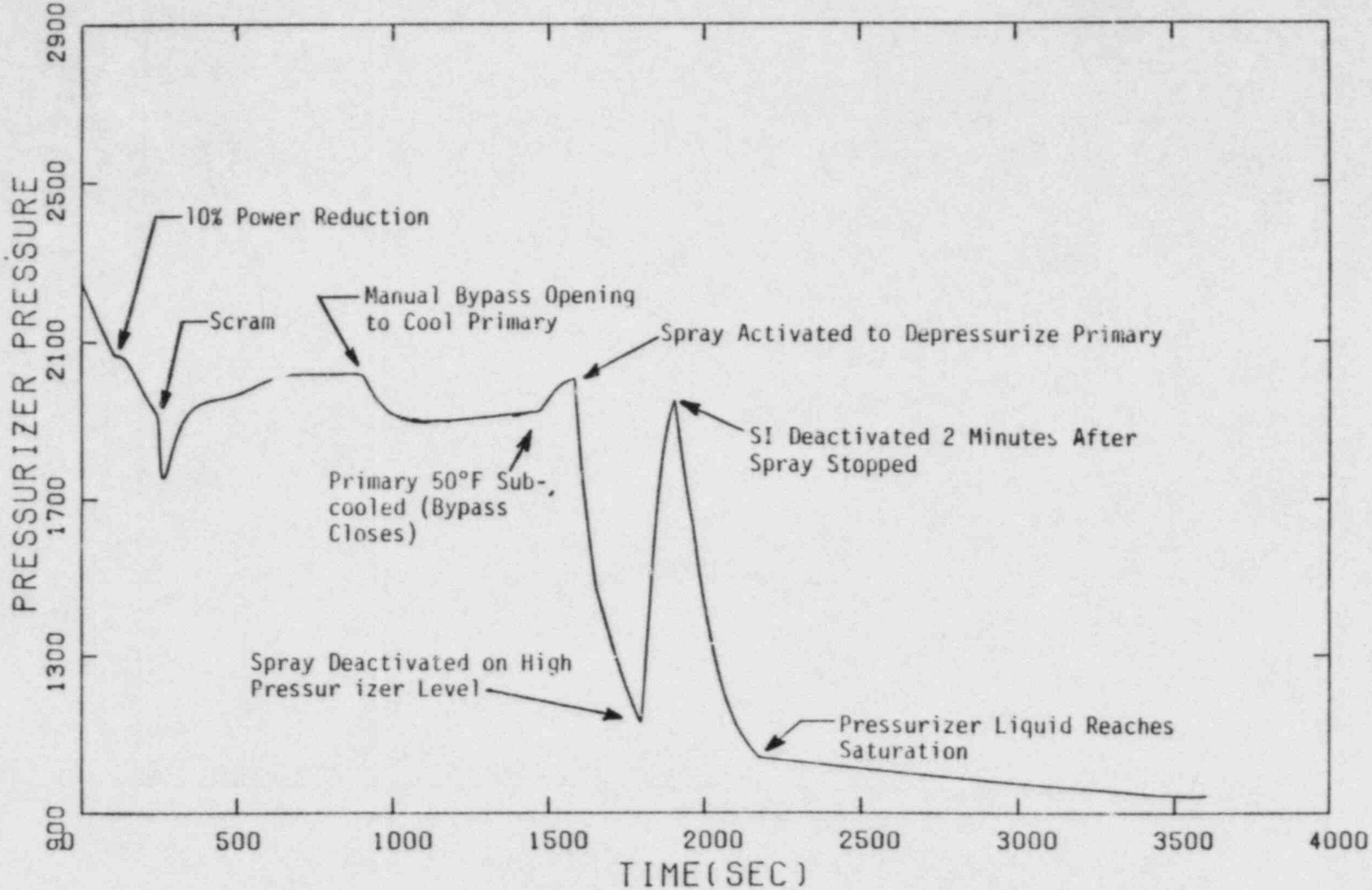



Figure 8. Pressurizer Pressure (psia) Versus Time (Sec)

# OTSG IODINE DF DURING SGTR

(ITI SINGLE SGTR CASE 3)

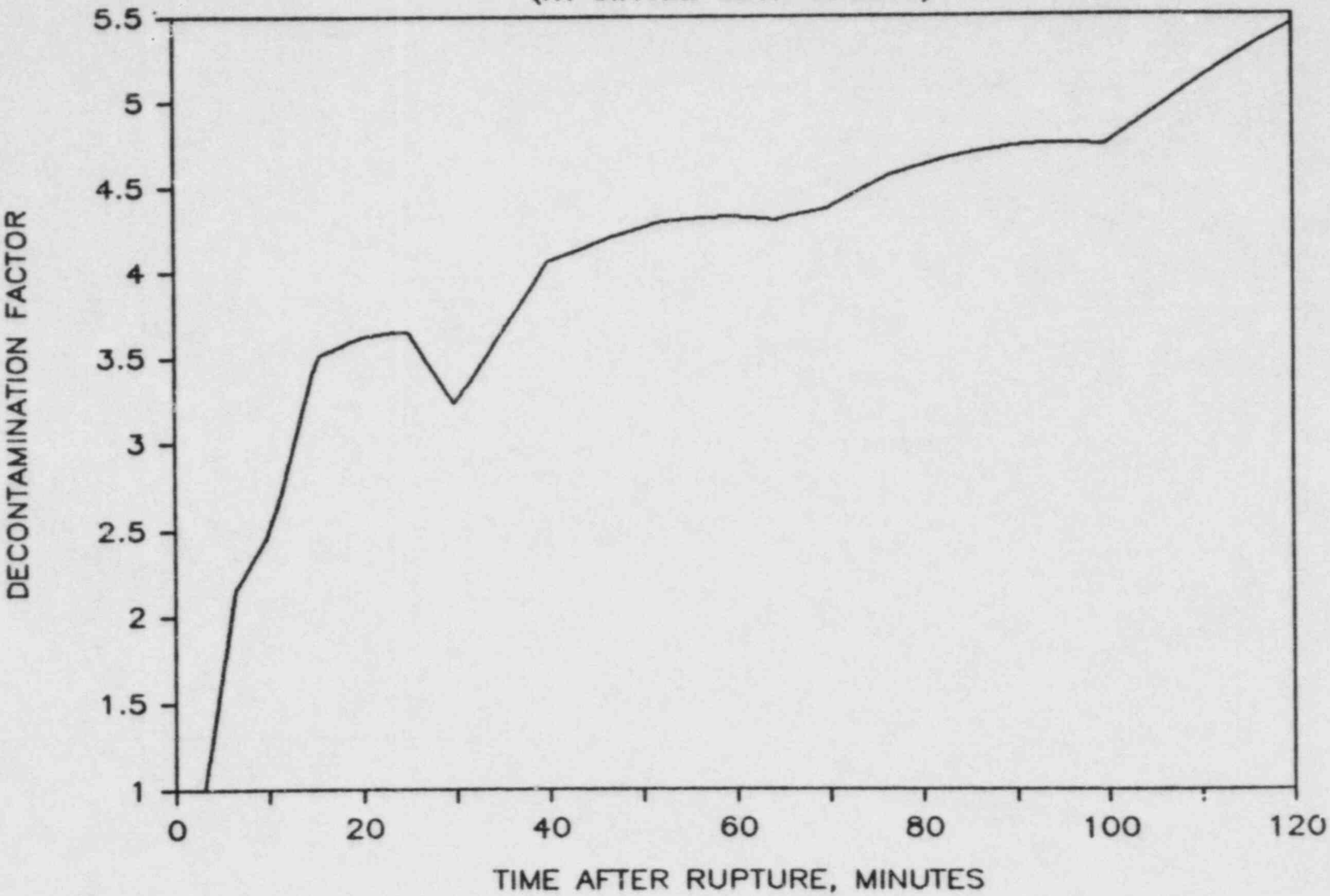
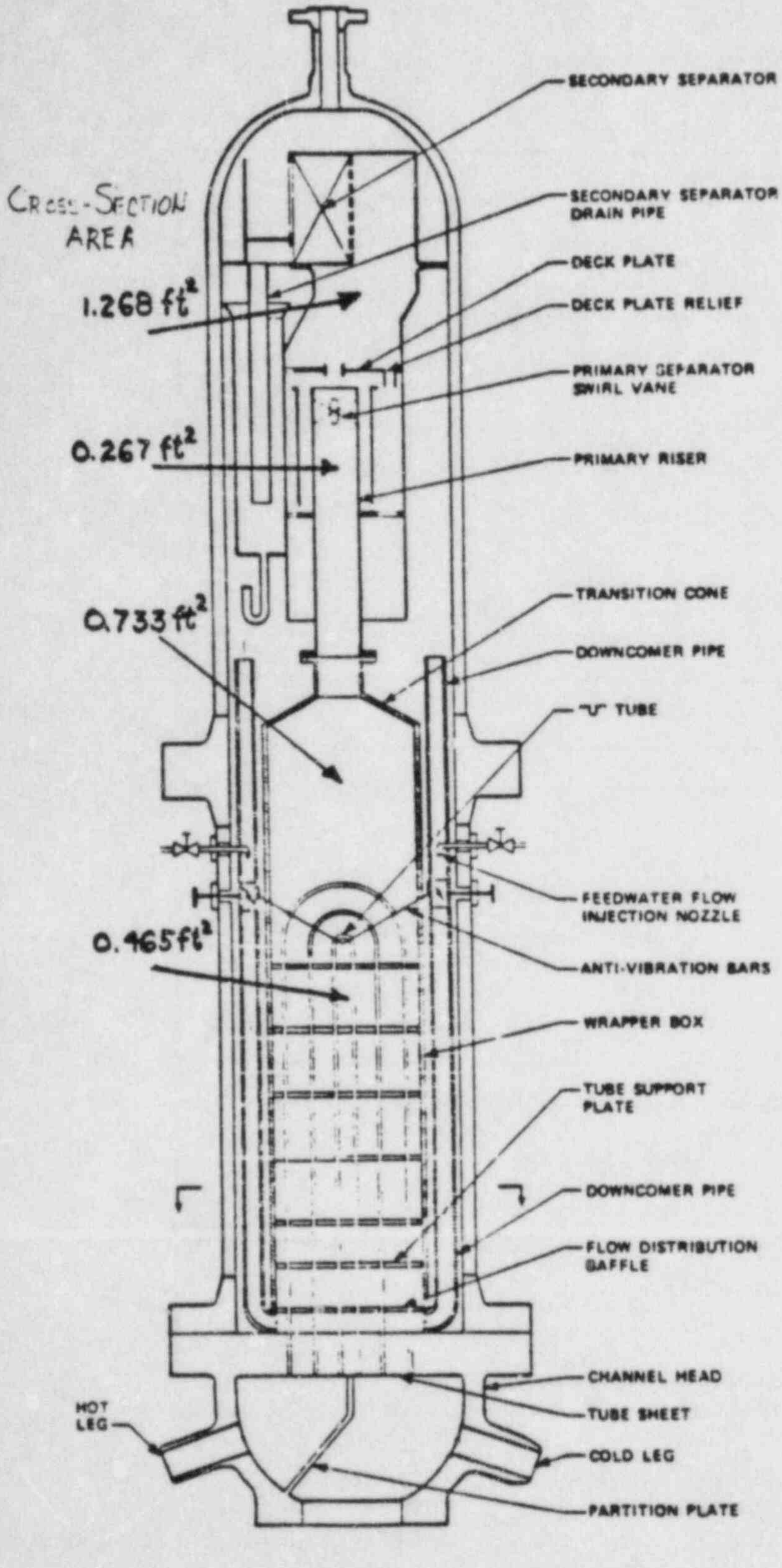
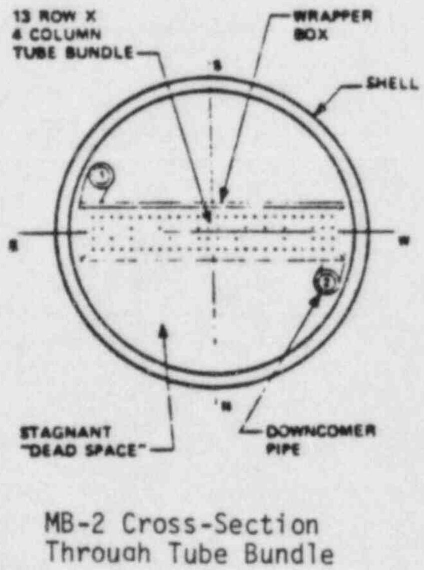



Figure 9




Model Boiler 2 Elevation




MB-2 Cross-Section Through Tube Bundle

Figure 10

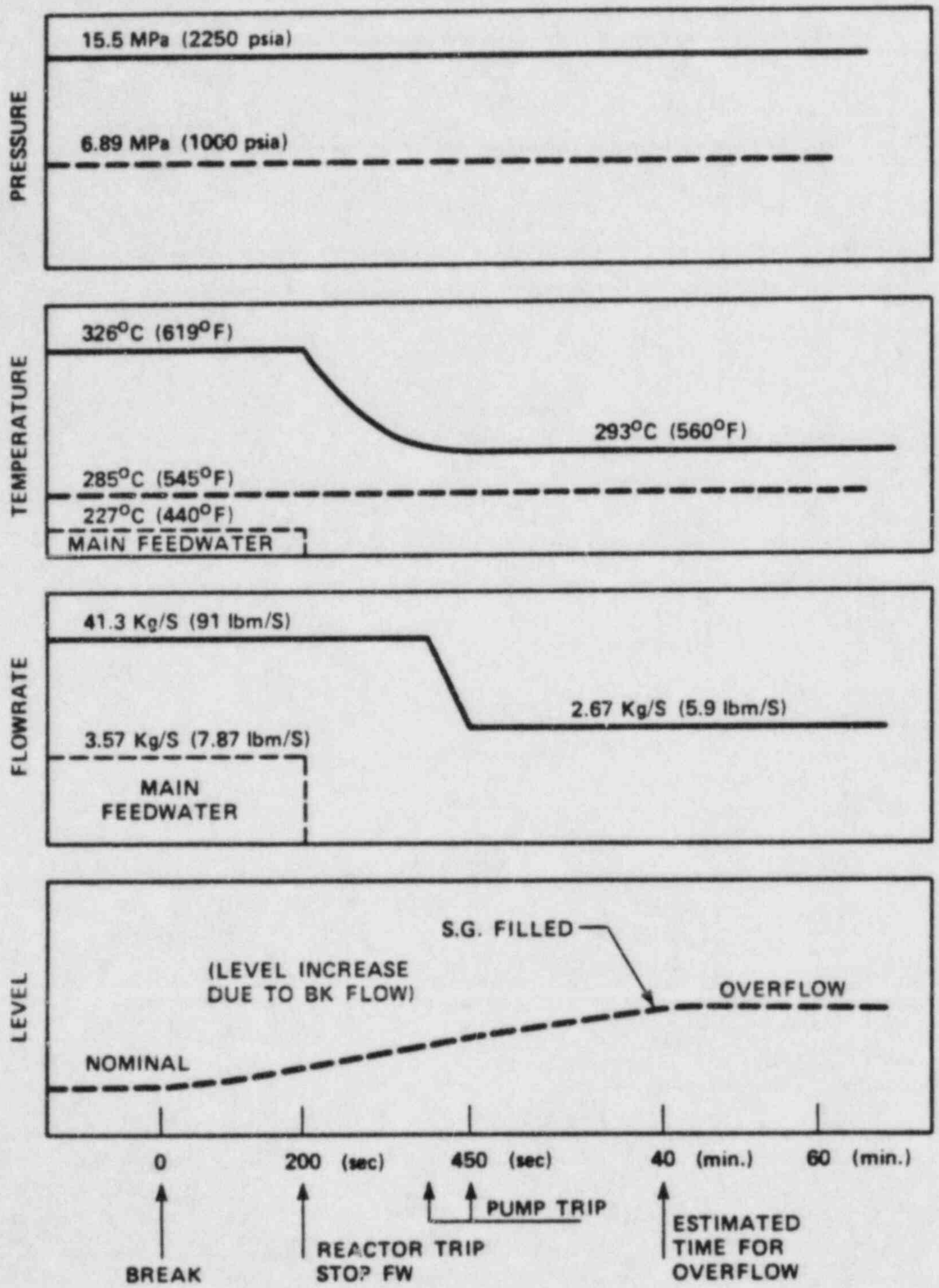



Figure 11. Steam Generator Tube Rupture Test Scenario

# SIGNAL VALIDATION: A NEW INDUSTRY TOOL

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and

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## INTRODUCTION

The instrumentation at nuclear power plants is relied upon to provide the operating staff with information on plant status and operating state. The validity of the information is totally dependent upon the validity of the electronic instrumentation system's (e.g., transducers, signal conditioning and read-out devices, etc.) performance. The utilization of existing instrumentation as the prime information source is further increasing due to the introduction of new computer-based systems in power plants. Fortunately, there is sufficient hardware instrumentation redundancy in the current nuclear power plants for measuring the key plant variables. Improving the reliability of the signals and efficient use of their diversity with the new and powerful plant process computers is a candidate research topic. EPRI's research projects are focused on reducing the instrument-related problems in the nuclear power plants by combining the traditional signal validation methods with the analytic relationships among the key plant variables.

One of the post-TMI requirements in the nuclear power plants is to install the Safety Parameter Display System, (SPDS), the purpose of which is to provide all pertinent plant status information in a concise form to the power plant operators.

Most utilities, while trying to meet the SPDS and Emergency Response Capability Requirements (ERC), have either replaced or are in the process of replacing the plant process computers with very powerful machines. In addition, the utilities have realized the benefits of installing the new computer-based operator support systems to help the crew work more efficiently, during both normal and abnormal conditions.

EPRI's Disturbance Analysis Surveillance System (DASS), Combustion Engineering's Critical Safety Function Monitoring System (CFMS), Westinghouse's Plant Safety Monitoring System (PSMS), are some examples of a wide variety of computer aids being developed in the nuclear industry. All of these computer-based systems are examples of increased reliance on existing plant instrumentation systems and further emphasize the urgent need for the industry to find the way and means to improve their reliability. To effectively demonstrate the signal validation projects' research results, EPRI selected the SPDS as the computer-based system, the reliability of which can be improved by enhanced signal validation methods. EPRI is working with the Northeast Utilities Company (NUSCO) and the General Public Utilities (GPU) to install the signal validation software as part of the Millstone-3 and Oyster Creek SPDS software, respectively.

## SIGNAL VALIDATION METHODS

In the current plants, the plant operating crew usually relies on judgment-based criteria for detecting signal failures, beyond the hardware redundancy implemented for selected safety-related signals. The operating crews at power plants have always performed some measure of manual signal validation traditionally. Redundant gauges are intercompared, and functionally diverse indications are correlated with operators' mental model of plant behavior. Today's powerful computers in the plants promise relief to the operators in this area by providing on-line signal validation automatically. The systems-like SPDS can be made robust to yield a low false alarm rate and high information reliability must be and can be assured at the front end by the on-line signal validation methods.

Most common signal validation practices in the nuclear industry today are limited to a few rather simple techniques that rely, for the most part, on process symmetry characteristics and physical redundancy of observables. These techniques include methods such as sensor comparisons, limit checking, auctioneering, instrument-loop integrity checking, and calibration checking. Since the time EPRI initiated a feasibility study using the parity-space and Analytic Redundancy techniques in 1980, many changes are appearing to take place in the signal validation process in the power plants.

Although no improved systems have actually been installed in the operating plants, many studies have been published utilizing advanced concepts such as the use of extended Kalman filters coupled to computationally intensive hypothesis testing and predefined failure modes or signatures. Even expert systems-like approach to signal validation involving a long sequence of IF-THEN rules are being implemented to compare the signals taking the plant state into account.

In the following paragraph, the general concepts used in EPRI signal validation projects are outlined and a brief report on each of the key projects is given. Detailed documents in individual projects may be obtained from the EPRI project manager.

METHODOLOGY

The parity-space and analytic redundancy techniques used quite widely in the aerospace industry are being applied with improvements in the EPRI projects for validating the key variables in the BWR and PWR plants. The parity-space algorithm makes use of consistency tests among all measurements of a given variable for fault detection and isolation (FDI) and the parity vector grows in the direction associated with a failed sensor. An estimate of the variable is obtained as a weighted average of the consistent sensor measurements. Consistency is defined relative to an acceptable error magnitude for each measurement. The methodology does not require detailed knowledge of sensor and plant noise statistics. The test is embodied in the decision/estimator (D/E) Code unit.

The signal validation process can be represented in terms of information flow. As shown in figure 1, the plant sensor generated information flows through a structure of D/E and Analytic Redundancy Model (ARM). The D/E is the OR-gate; the ARM symbol is the AND-gate. The particular D/Es and ARMs and their interconnection is a function of plant configuration, sensor signal availability on the process computer and the design requirements.

The Analytic Redundancy refers to the measurement of a variable using the physical relationships among other variables. The ARM uses functionally diverse data inputs to derive an analytic model that generates a signal output. This output is sometimes referred to as an "analytic measurement." ARMs are introduced for several reasons. Firstly, there may not be sufficient redundancy among identical, colocated sensors to perform the fault detection and isolation (FDI) function of the D/E. In these cases, the ARM will enable the use of other sensors to supplement inadequate direct or physical redundancy. Secondly, ARMs can provide a measure of common-cause rejection by synthesizing a signal from functionally diverse sensors. ARMs can generate signals that are not currently instrumented or that are difficult to observe. Finally, ARMs provide a measure of component FDI since they implicitly model the normal operation of a component.

The basis for ARMs is mass, momentum, and energy conservation, hardware operating characteristics, and empirical correlations. The models are low-order, without any restriction to linearity, and may contain parameters that are fitted to plant sensor data either off-line or adaptively. Because the models are sensor driven and limited in scope, fairly simple models suffice to give good fidelity. If the sensor data update interval is short relative to process time constants, then static models can be used even in transient operation.

The decision/estimator is the core of the FDI logic. It is a totally generic process, programmed or coded as a generic software unit wherein two or more data sources are algorithmically merged into a single data output. Further, a rule-based decision logic is incorporated. The data sources to a D/E must all be of the same type, as for example, pressure sensor data from identical colocated sensors. These data sources may have different ranges and accuracies, and may derive from any of the following three sources. The data inputs to a D/E may derive directly from sensor data, from the output of another D/E, or from the output of an ARM. The generic term "signal" applies to all cases, and the term "validated output" applies to the D/E output. All signals are ultimately derived from sensor data, and hence all signals will contain bias and scale factor errors with respect to the true value of the underlying plant variable. Some signals will be out-of-range and others may be failed at any particular sample time. A failed signal is defined as a signal which departs from plant truth by greater than the normal bias and scale factor errors. Failed signals may nevertheless be on-scale.

The D/E performs two functions. First, a decision is made as to which inputs are consistent and to what degree. Failed instruments are detected, identified, and isolated from the second function, which is the generation of a "validated estimate" output. The mathematical basis for the D/E is documented in several EPRI reports.

Because it is important to provide some information at the output of a D/E under all circumstances, it is necessary to consider a number of logical cases and to provide rule-based alternatives for different applications. For example, the cases where there are two or more unfailed on-scale measurements that are totally consistent (maximum $I_i = 0$) or partially consistent (at least one $I_i = 0$) are relatively straightforward. When there is only one unfailed, on-scale measurement, that measurement is checked for consistency with any unfailed but range-limited measurements. If there is an inconsistency, then a rule-based procedure is invoked.

Although consistency is determined solely on the basis of current observations, fault declaration and subsequent removal of an input is based on a sequential test using cumulative information from past and current samples. This is necessary to keep error bounds small enough to minimize the probability of missed alarms and at the same time avoid excessive false alarms from spurious noise, modeling error for analytic inputs, and miscalibration. A simple approach to sequential testing that is computationally not demanding and does not rely on intrinsic assumptions regarding measurement noise is used.

In the pump example of figure 1a, the available sensor information is given. The objective of the signal validation in this example is to provide valid flow measurement, using validated measurements as much as possible. First, the two flow sensors F1 and F2 are used in the INT A D/E logic to provide an intermediate Flow estimate $F_{INT}A$. The three discharge pressures, $P_{D1}$, $P_{D2}$, and $P_{D3}$ are used to derive a validated discharge pressure $\hat{P}_D$. An analytic redundancy model for the pump uses the diverse pump section pressure, the speed sensor and the validated discharge pressure as the inputs to generate another set of flow estimates. These estimates, together with $F_{INT}A$ estimate, are used in another decision/estimator logic to get a better flow estimate $F_{INT}B$, which is then compared with the flow sensor measurements F1 and F2 in a final D/E to get a single validated flow measurement

As in the example, the information from the actual measurements are always given higher weights compared to analytic measurements in estimating their variable estimate. When there are adequate sensors, as in the case of the discharge pressure, no analytic models are used and the conventional like-sensor comparison method with rule-based procedure incorporated in the D/E logic provides an improvement to detect the sensor faults.

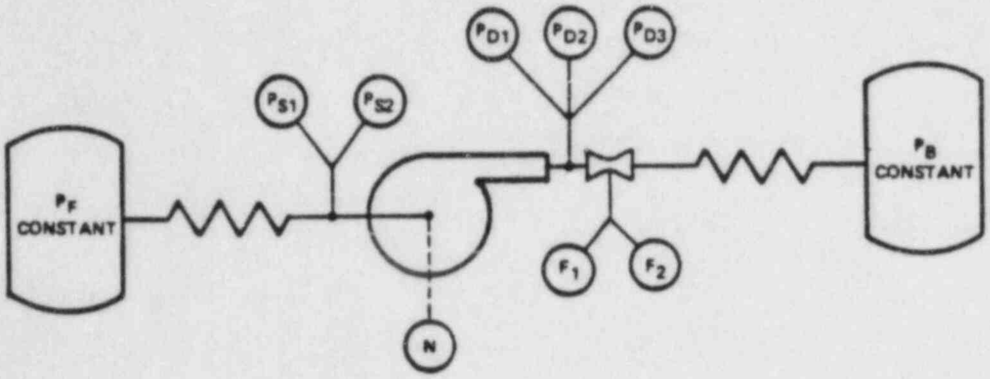



Figure 1a. Example Illustration of a Pump and its Measurements

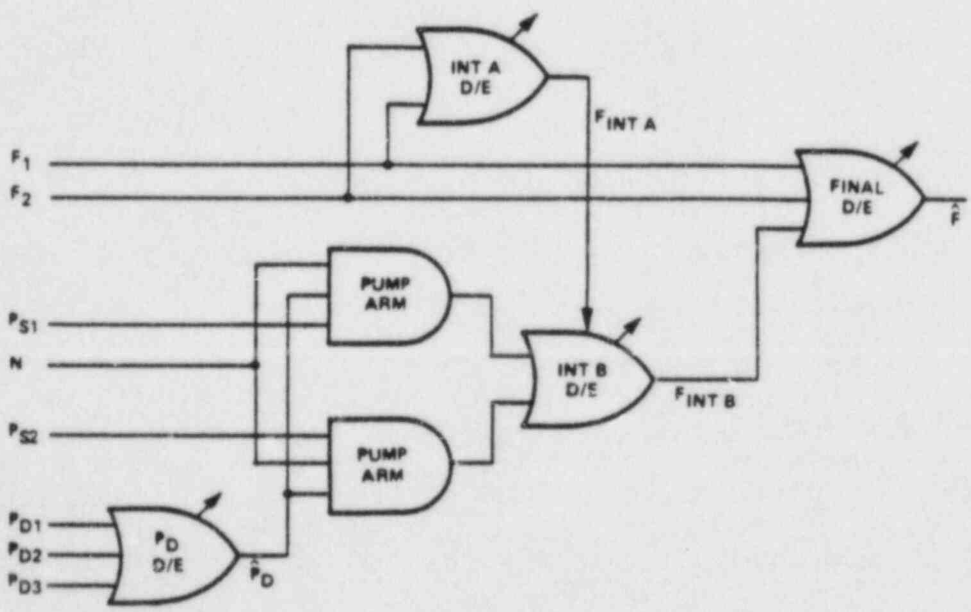



Figure 1b. Signal Flow Diagram for the Pump Example

## EPRI PROJECTS

### Steam Generator/Feedwater Subsystem Feasibility Study

The general design approach using the parity-space representation and analytic redundancy techniques was first demonstrated and reported in a feasibility study (EPRI NP-2110) using the Steam Generator/Feedwater Subsystem of Baltimore Gas & Electric's Calvert Cliff Unit 1 plant. The sensors subset used in the study reflected the sensors in place at the Unit with some exceptions. The signal validation flow diagram and the models were developed to isolate failures at the sensor level in the subsystem, in addition to the components. A computer code (C-E ZAMB03) developed by the contractor, Combustion Engineering, was used to calculate accurately the test cases for the signal validation models to represent the subsystem behavior during the steady-state and transient conditions. The subsystem variables were corrupted with typical, uniformly distributed sensor noise to simulate sensor outputs, and the capability of the methodology to detect common-mode failures, bias and scale-factor errors and sticking values was demonstrated.

### Suppression Pool Parameters in a BWR

The signal validation methodology and the software developed in the feasibility study was first applied to monitor the Boiling Water Reactor (BWR) suppression pool parameters and related sensors, for the General Public Utilities' (GPU) Oyster Creek plant. As an outcome of this effort, contractors

C.S. Draper Laboratories and Nutech Engineers have developed a real-time signal validation program to validate the sensor signals for suppression pool temperature, level and pressure including the status of the safety/relief valves. The analytic measurements derived include: the Saftey Relief Valve (SRV) position from the temperature rise in quencher bays, total SRV steam flow using the Moody equation, total SRV steam flow from change in steam flow/feed-flow mismatch, containment spray system flow from pump discrete signals and four different estimates of the suppression pool bulk temperatures. The top level signal validation flow diagram is shown in figure 2, which accomodates both the current and planned upgrades to the pool instrumentation, and provides a single validated measure for the bulk temperature. The signal validation in this case was tested (EPRI NP-3641) using the test cases constructed from RETRAN and CONTEMPT code runs and the steady-state data.

The suppression pool temperature is an important safety parameter in BWRs, and it is used to initiate various operation actions such as suppression pool cooling, scram, and depressurization, as determined by the Emergency Operating Procedures. Also, the NRC has determined that a suppression pool temperature monitoring system (SPTMS) is required to ensure that the suppression pool temperature is within allowable limits in NUREG-0783. In support of a reliable SPTMS, the real-time signal validation program developed during this study, for monitoring the BWR sensors, offers the following potential benefits:

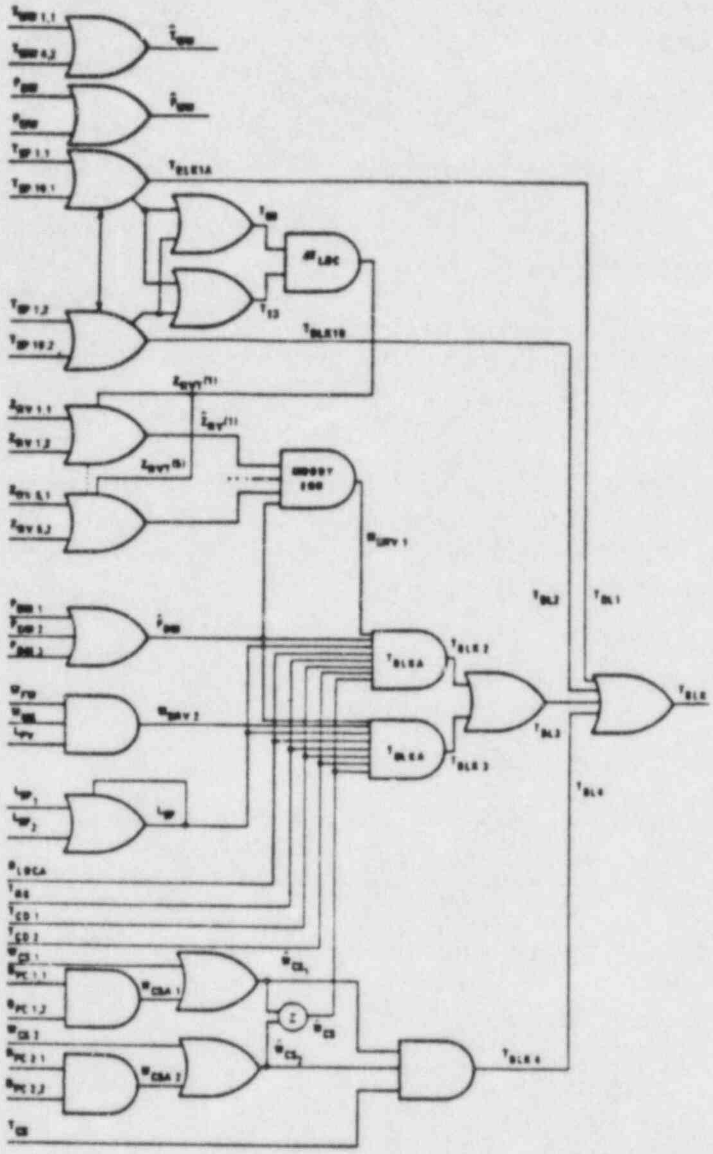



Figure 2. Top Level Signal Validation Flow Diagram

Legend

**Variables**

T-Temperature
P-Pressure
L-Level
F-Flow
D-Discrete Signal

**Subscripts**

WW-Wet Well
DW-Dry Well
SP-Suppression Pool
RV-Relief Valve
DM-Dome
FW-Feedwater

MS-Mainstream

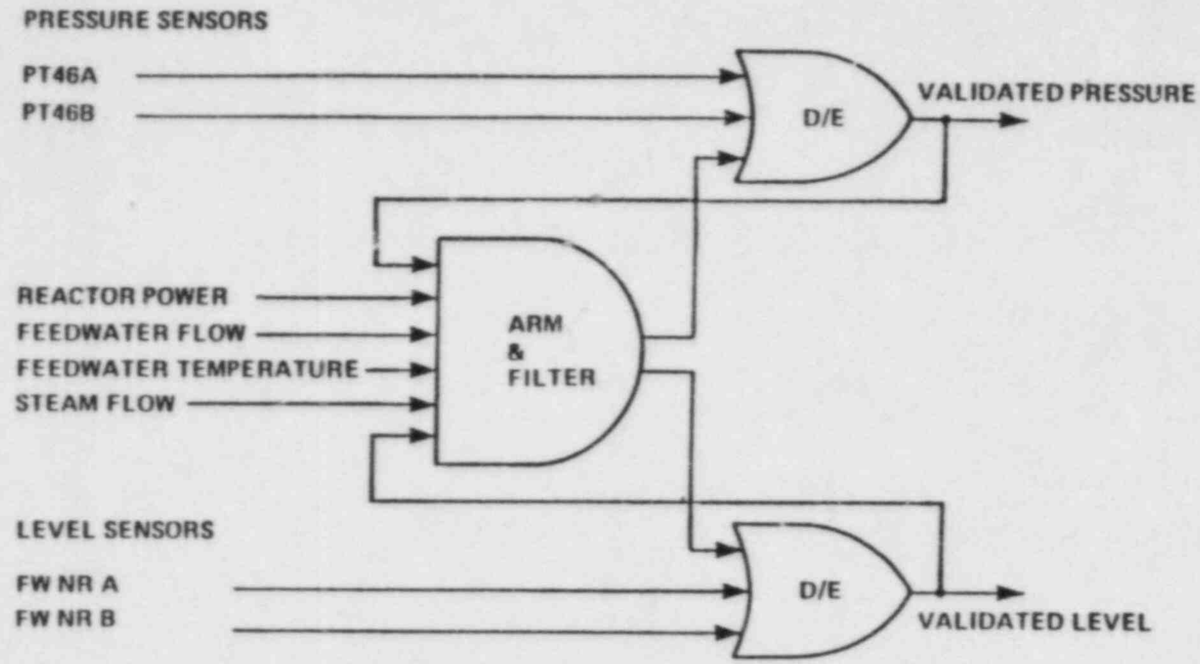
MS-Mainstream
PV-Pressure Vessel
RB-Reactor Building
CD-Containment Spray Discharge
CS-Containment Spray
PC-Containment Spray Pump

- Reduced probability of operator misactions such as delayed RHR initiation or inadvertent actuation based on inadequate indications.
- Increased plant availability resulting from allowing continued plant operation with fewer or failed sensors and at higher pool temperatures because of increased redundancy and accuracy.
- Potentially very significant and important economic incentives because of the reduced need for additional instrumentation, such as may be required by Regulatory Guide 1.97, NUREG-0783, and NUREG-0661.
- Detection of Common Cause Failures by use of diverse analytic and direct measurements of suppression pool bulk temperatures, relief valve positions and cooling flow rates.

BWR SPDS Support

EPRI is currently pursuing to develop the signal validation effort for other key parameters supporting the SPDS functions using GPU's Oyster Creek as the reference plant. The major part of this effort is to validate the BWR vessel level both inside and outside the core shroud, using the extended Kalman filtering approach. Figure 3 depicts the Oyster Creek vessel level and pressure signal validation procedure. The variables used in the suppression pool and BWR vessel validation are combined into a single libarary of software modules that would provide the signal validation for the Oyster Creek plant. The variables validated are:

1. Flow through reactor core
2. Core spray system flow
3. Reactor level
4. Reactor vessel pressure




ARM ANALYTIC REDUNDANCY MODEL

D/E DECISION ESTIMATOR

Figure 3. Oyster Creek Reactor Vessel Level & Pressure Signal Validation Diagram

5. Suppression pool level
6. Suppression pool temperature
7. Drywell pressure
8. Drywell temperature
9. Emergency relief valve positions
10. Neutron flux in core
11. Containment building pressure
12. Drywell sump level
13. Mode switch
14. SCRAM demand
15. Isolation demand

Measurements used in the validation designs were taken from the lists of inputs to the plant computer or were derived from the Oyster Creek P&ID book. The rule for determining which plant sensors were available for use in signal validation was the availability of sensor termination in the control room area. This rule was used in place of the restriction to sensors currently on the Oyster Creek computer interface to support a more generic design requirement. Further, plant sensor information from three of the Commonwealth Edison's BWR units were used in developing the generic software to validate the SPDS plant variables.

PWR SPDS Support; Validation and Implementation at Millstone-3 and Millstone-2 Plants

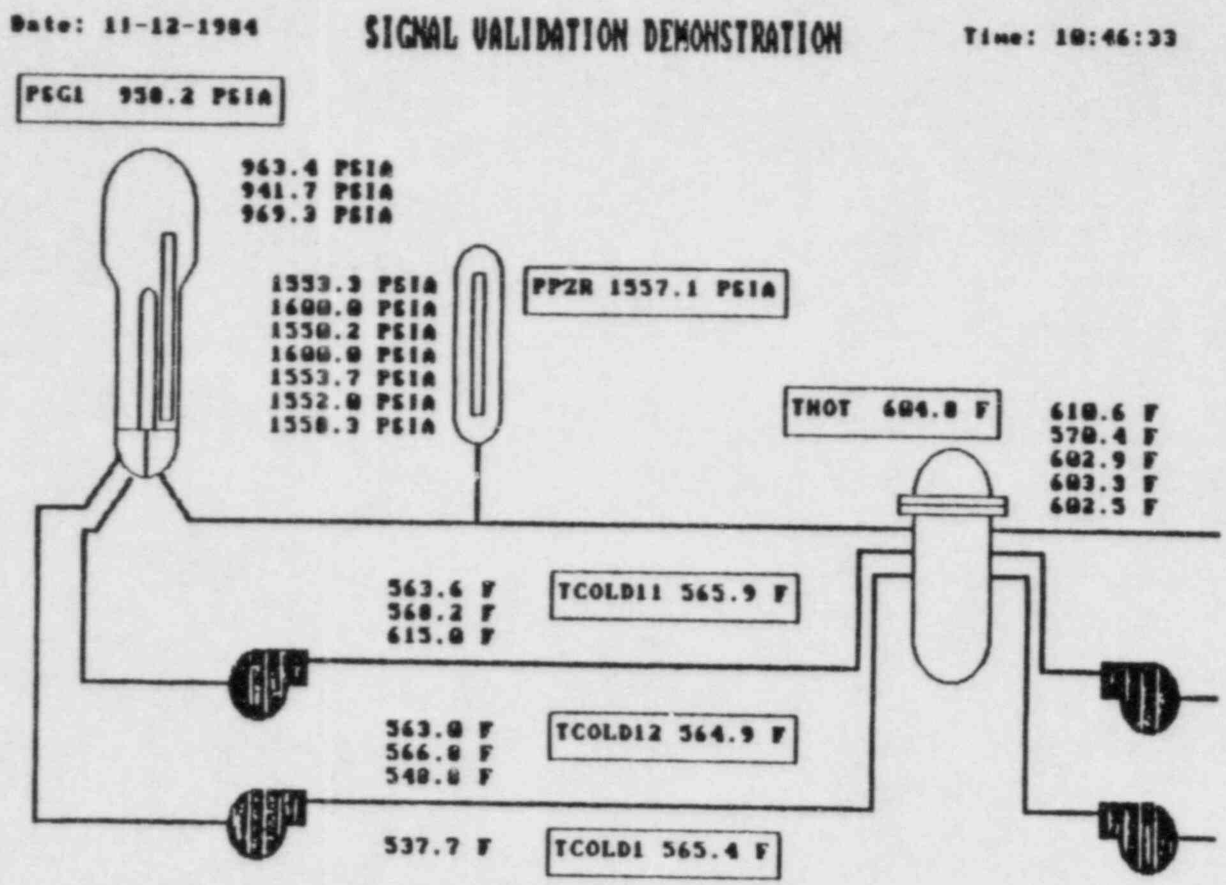
Working with nine utilities, EPRI developed a list of eighteen key variables for which the signal validation is necessary and that would support the critical safety functions used in the SPDS in the operating PWRs. Babock & Wilcox and C.S. Draper Laboratories are the contractors in this project to develop and test an on-line signal validation software for the Westinghouse and Combustion Engineering plants. As a part of this project, the signal validation functional requirements and design specifications documents are completed. The reports summarizing the testing of the software at the Northeast Utilities' Millstone-3 and Millstone-2 plants are scheduled to be completed in mid-1986 and mid-1987 respectively. When completed, the software will be available in modular form with individual test cases for each plant variable for use on the IBM-PCs.

The project results to date have been encouraging. Three demonstrations have been made under the project auspices. The first demonstrated the techniques of signal validation for a feed pump's instruments along with the response of the validation software upon inducement of instrument failures. The second demonstration used system level analytic models in an observer technique for inferring natural circulation flowrate and cold leg temperature for a Westinghouse type plant. That demonstration showed how advanced techniques can be used to generate an independent measurement of a nonmeasured variable. The final demonstration used the modules which have been developed for the Millstone-2 plant. Simulated data was transmitted through a data acquisition system into a process computer. Instrument failures were simulated and the correct response of the signal validation modules were observed. These demonstrations have shown that signal validation can be used successfully in nuclear power plant applications.

This project, which will demonstrate signal validation at Millstone-3 and Millstone-2 host sites will use a subset of the critical safety functions defined for that plant. These critical safety functions are: core cooling, reactor coolant system heat removal, reactor coolant system integrity, containment integrity, reactivity control and reactor coolant system inventory. The variables that were selected for validation are:

Reactor Coolant Hot Leg Temperature
Reactor Coolant Cold Leg Temperature
Reactor Coolant Pressure
Reactor Coolant Flow
Residual Heat Removal Flow
Steam Generator Level
Steam Generator Pressure
Feedwater Flow
Neutron Flux
Pressurizer level
Letdown Flow
Emergency Sump Level
Containment Pressure
Core Exit Temperature
Control Rod Position
Subcooling Margin
High Pressure Injection Flow
Low Pressure Injection Flow

Figure 4 shows the effect of the plant specific signal validation schemes in identifying the faults in the pressurizer pressure (PPZR), reactor coolant system hot leg




3 D/E's - A Analytic
Measurement & Pumps

Figure 4

temperature (THOT), and cold leg temperature (TCOLD) and the steam generator pressure (PSGI). In the figure:

-- The steam generator pressure module has a partially consistent set of measurements. That is, measurement 1 and measurement 2 are inconsistent with each other, but they are both consistent with measurement 3.

-- The hot leg temperature module also has a consistent subset of measurements. Measurement 2 has failed low and is excluded, but the remaining measurements form a consistent subset.

-- The results from the pressurizer pressure module with three different sensor ranges are given. Measurements 2 and 4 are low range 0-1600 psi sensors that are pegged high and removed from the estimate.

-- The results from the reactor coolant cold leg temperature module are depicted with three decision/estimators, an analytic measurement and the use of the pump status to determine the quality of the intermediate estimate varies as measurements fail, first one high, then one low.

## CONCLUDING REMARKS

The signal validation is an essential element in the development or utilization of any computer-based operator-aid system. Immediate potential applications are in verification of input signals to the SPDS and plant controllers. The parity-space and analytic redundancy techniques employed in the EPRI projects incorporate the conventional, first-level validation checks, wherever possible. The use of analytic redundancy offers a cost-effective alternative to additional sensor hardware and allows the detection of both common-mode sensor failures and the failure of non-sensor components, such as valve stroking mechanisms, pumps, and analog controllers that are modeled in the analytic relationships. Although its fault isolation resolution is dependent upon the particular application, the proposed technique allows the rapid detection of the failure of one or more components within a set of relatively few components.

Investigations into causes of unavailability at PWR plants have shown that for an average plant having about 30% unavailability, instrument failures alone account for one trip per year per plant and the average plant shutdown time is around 24 hours. The data shows the variety of sources of sensor failures and the dramatic effect these failures have had on power productivity in terms of plant down time. If such spurious trips could be eliminated via a sensor validation method, a savings of about $500,000 per year per plant could be realized by a utility, based on typical replacement power costs for an 800 MW(e) unit for one day. In addition, avoidance of such use of erroneous sensor information would dramatically reduce unnecessary challenges to plant protection systems, thereby providing a significant safety enhancement. It must be realized that the instrument-failure-induced trip frequency has occurred in the presence of one or more of the techniques used currently. Therefore, it is evident that not only will a more precise and robust sensor validation technique would enhance plant safety, but it would also be cost beneficial as a result of increased plant availability. Furthermore, the potential of the methods used in EPRI signal validation projects to identify failures of non-sensor plant components promises additional gains in safety and availability by permitting timely and accurate operator action to alleviate these disturbances.

# ON-LINE CORROSION CRACKING MONITOR DEVELOPMENT

by

J. D. Gilman and R. L. Jones

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ABSTRACT

On-line measurement of crack growth in nuclear plant operating environments is technically feasible using an electrical potential measurement technique. Applications to corrosion crack growth monitoring are suggested because the majority of service-induced cracks are environmentally-assisted. Surveillance of existing cracks at piping welds, using non-intrusive instrumentation, is a relatively well developed application of the technology. A more comprehensive on-line monitoring system would utilize internal sensors having well defined cracking characteristics. A computer-based data processing capability can be coupled with predictive models of corrosion cracking processes, so that short-term and cumulative damage rates at crack locations can be displayed in real time. It is suggested that component life extension and maintenance cost reduction can be achieved through plant operator interaction with a well-developed corrosion cracking monitor system.

## INTRODUCTION AND SUMMARY

Corrosion-assisted cracking of LWR components is a major cause of forced outages with associated high costs for repair or replacement of affected components. When cracks are discovered, plant owners must choose between repair or replacement alternatives, or they may elect to defer this decision on the basis of evidence that the plant is safe to operate. Immediate replacement using a crack-resistant material may be extremely costly and impractical, so attention is focused on means of limiting, predicting, and monitoring the rate of growth of small existing cracks. A reliable assessment of future crack enlargement is necessary to support a decision to defer repair or replacement. Similarly, if a component is repaired without removing the crack, a crack growth assessment is usually required.

A long-term research emphasis on these corrosion cracking problems has necessitated the development of sensitive techniques for crack growth measurement in high temperature water environments. One crack-following method which has been used extensively in the laboratory is also finding useful applications in operating plants. By monitoring instrumented cracks which have been introduced directly into the reactor environment, operators can determine (for example) the effectiveness of water chemistry controls in suppressing stress corrosion cracking. Known cracks in plant components, such as stress corrosion cracks in piping welds, can also be instrumented and monitored. Future development goals envision plant operator interaction with a damage monitoring, prediction and display system, leading to component life extension and maintenance cost reduction.

## REVERSING DC ELECTRICAL POTENTIAL TECHNIQUE

Electrical potential techniques in many variations have been used for crack growth measurements, both in the laboratory and in field applications. The common feature of these techniques is an array of electrical probes in the vicinity of the crack that sense small voltage changes resulting from distortion of a potential field by the crack. Both alternating current and direct current systems have been developed, and each has advantages peculiar to different applications.

A "reversing dc potential monitoring technique" developed at General Electric was applied as early as 1978 in an EPRI study of crack growth in carbon steel.(1) Later, the method was adapted to the study of surface "thumbnail" cracks in laboratory test specimens.(2) A calibration model converts voltage

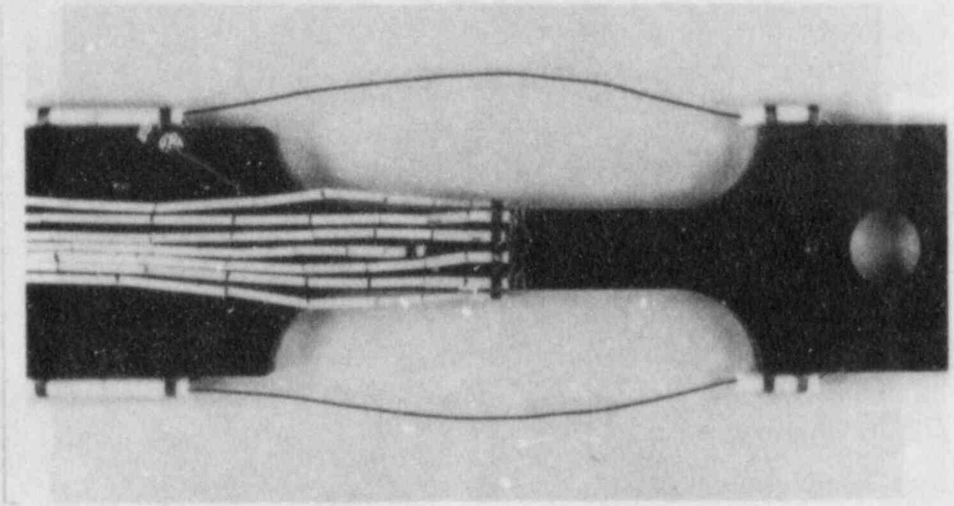

Figure 1. Laboratory test specimen fitted with potential monitoring probes.

differentials between various pairs of surface-mounted probes (Figure 1) to an estimate of the size and shape of the surface crack. Good crack-size estimates are obtained for cracks that are roughly semielliptical (Figure 2), using the mathematical description of the potential field surrounding an ellipsoidal cavity. An algorithm selects the ideal semiellipse that best fits the potential readings.

Nuclear plant monitoring requires a measurement system suitable for remote operation, with long-term stability that is not influenced by temperature changes. Several innovations and refinements make the reversing dc system (Figure 3) unusually stable and sensitive. Crack growth rates of the order of a few thousandths of an inch per year can be resolved by some operational laboratory systems. Thermoelectric effects are compensated by reversal of current polarity at half-second intervals. Multiple potential readings (typically 16) are made before and after each current reversal, and these are averaged to enhance the signal-to-noise ratio. The potential readings for active probes are normalized with respect to a reference probe potential to compensate for changes in current or in temperature-dependent material properties.[3] Use of direct current permits the use of long, unshielded leads between the probe attachments at the component and the amplifier or nanovoltmeter at which the potential is measured.

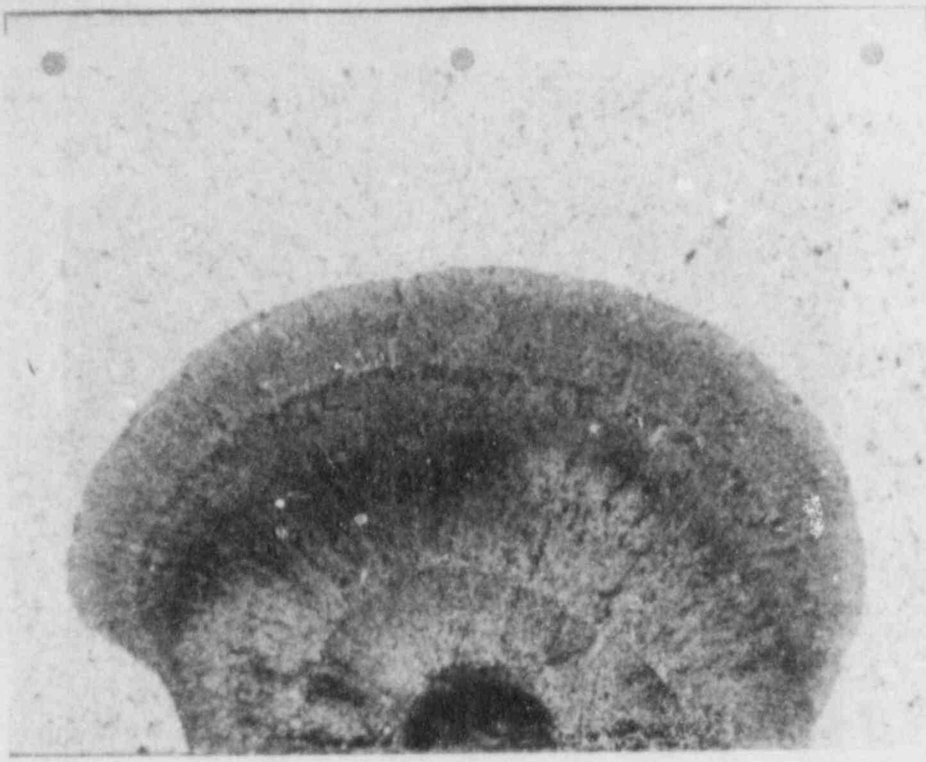

Figure 2. Fatigue crack propagation from an initially semicircular defect in a laboratory test specimen.

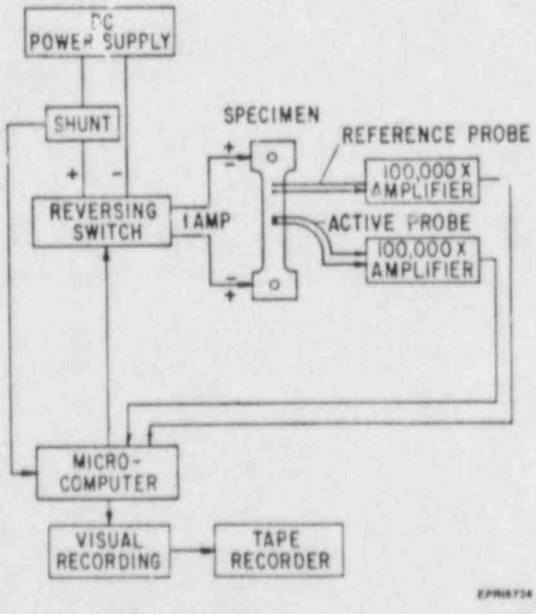


Figure 3. Reversing DC Electrical Potential Crack Monitoring System.

PIPE CRACK MONITOR

Laboratory measurement of the growth of internal pipe weld defects has been accomplished by attaching probes inside the pipe near the crack face, but the procedure is difficult at best and inapplicable in the field. One objective of this test program was to develop a way to monitor an internal pipe crack using external, nonintrusive electrical probes.(4) An analytical study showed that surface cracks in a pipe or plate could be monitored with a useful degree of accuracy using probes attached to the surface opposite the crack. The crack must be located using another technique such as ultrasonics, and the surface-mounted leads must be accurately located with respect to the crack. Given these conditions, the electrical potential difference across an external probe pair is quite sensitive to depth increase of a long, shallow crack, having an a spect ratio of 0.01. (Figure 4) The technique is less sensitive to growth of a semicircular defect, having an aspect ratio of 0.5, but the latter is of much less consequence to the structural integrity of the piping system.

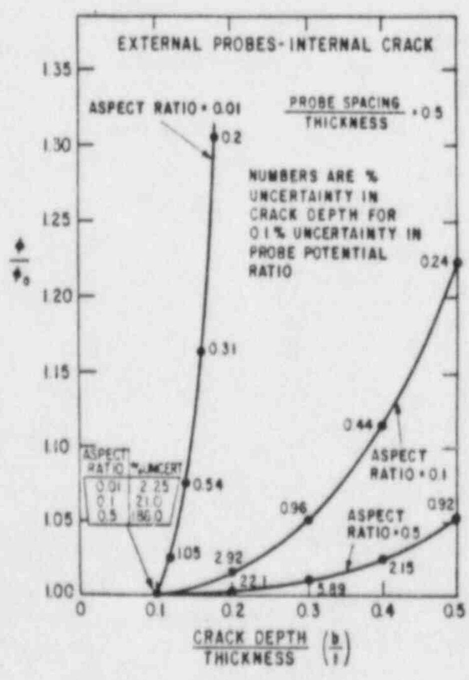


Figure 4. Normalized electrical potential at probes attached to the surface opposite to a surface crack.

In a laboratory demonstration at General Electric's Corporate Research and Development Laboratory in Schenectady, internal defects in a 4-inch stainless steel pipe were propagated by low-cycle fatigue while the pipe was filled with 290°C water. Crack enlargement measured with external probes showed good agreement with the crack size derived from internal probes (Figure 5) and from post-test destructive examination.[5]

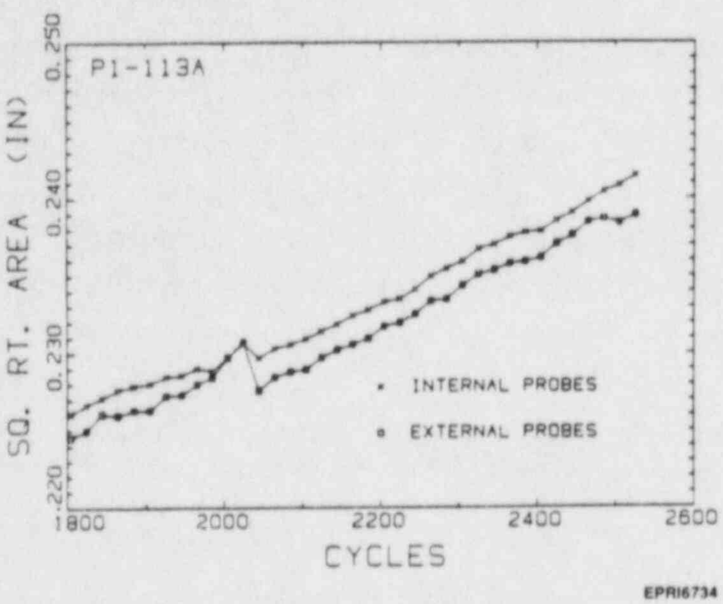


Figure 5. Enlargement of a pipe crack as measured by internal and external potential monitoring probes.

Figure 6 shows a 4-inch pipe in GE's Pipe Test Laboratory in San Jose, equipped with external probes at an internal corrosion crack location. The material and environment are conducive to intergranular stress corrosion cracking (IGSCC). Tests currently in progress show a gradual increase of potential during periods of constant loading that is indicative of IGSCC. These tests are part of a project for assessment of degraded pipe repairs that is being conducted for BWR Owners Group-II.[6]

Techniques for monitoring small pipe must be modified for application to large pipe in field applications, because it is not practical to pass a uniform

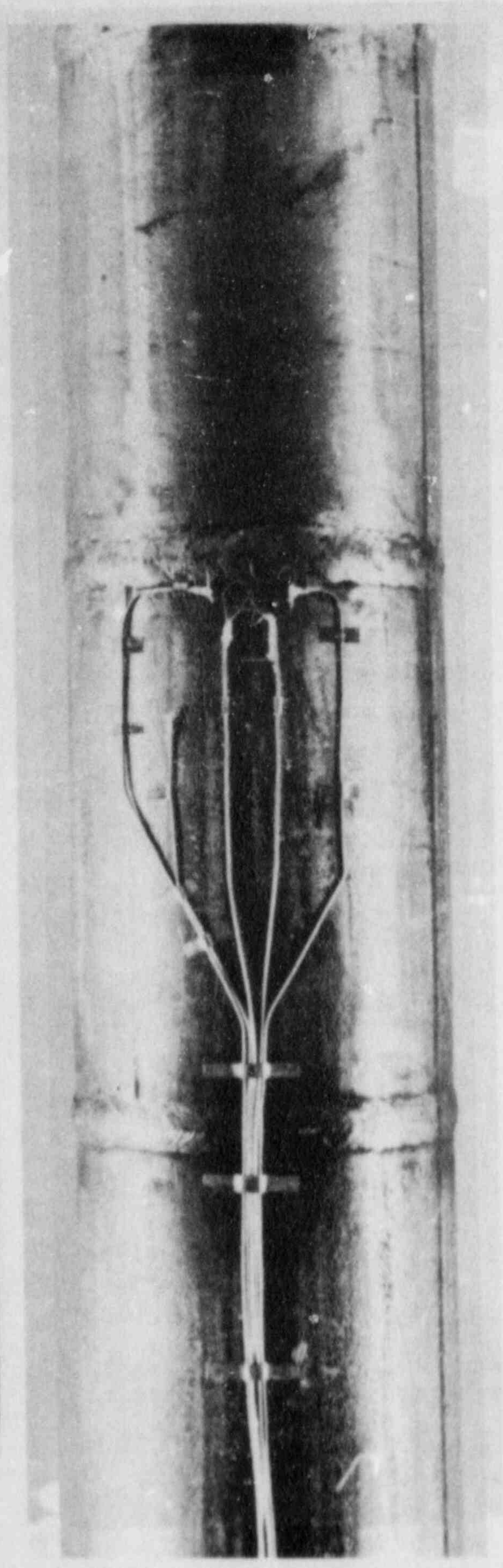

Figure 6. Potential probes attached to a 4-inch pipe for monitoring of IGSCC during simulated service testing.

current down a large pipe to create the potential field. The current source and sink must be located close to the crack, to keep current requirements low. Current work in the Owners Group project will optimize probe locations and modify the analytical calibration model as required for large pipe applications.

Many IGSCC pipe cracks in operating plants have been reinforced by application of a weld overlay, and there is incentive to monitor representative repairs in the field to confirm the satisfactory long-term performance of this repair technique.(7) Monitoring a crack beneath a weld overlay repair requires some further modification of the large-pipe calibration model to accommodate the thickness discontinuity in the pipe wall. A laboratory demonstration of IGSCC monitoring of a 12-inch weld overlay repair is planned in early 1986.

The calibration model for pipe crack monitoring characterizes the crack as the semiellipse for which the potential measurements most nearly conform to the mathematical calibration model. Cracks having well-defined length and depth should be adequately represented by this idealization. If the crack shape is very irregular, as it may be for some IGSCC pipe cracks, a source of error is introduced. The accuracy of the pipe crack monitor in practical field applications is likely to be limited primarily by this factor.

## STRESS CORROSION CRACKING SENSOR

The behavior of hypothetical or inaccessible cracks in reactor components can be assessed with a stress corrosion cracking sensor of similar material and exposed to the same enviroment. For example, standard fracture mechanics specimens of several materials have been installed in an autoclave in Commonwealth Edison's Dresden 2 plant and exposed under stress to reactor coolant.(8) Crack growth data measure the effectiveness of hydrogen water chemistry in suppressing stress corrosion cracking in these materials. These sensors aid in establishing the limits of benign water chemistry conditions, and they could also serve to measure the extent of SCC damage due to transient off-normal water chemistry conditions. Similar installations are planned in at least two other domestic plants, and in a Swedish plant.

An alternative to in-plant installation of an autoclave and loading mechanism is a preloaded, precracked sensor that can be inserted directly into the reactor at a location that provides an environment similar to that of the component of interest. The sensor must be mechanically and functionally reliable, and it should also be compact and self-loaded. A wedge-loaded double cantilever beam sensor has been designed to meet these requirements.(9) Side grooves maintain the crack in its plane, and a series of potential probe pairs along the beam provide a very sensitive indication of crack extension. The size, shape and dimensions of the sensor are selected to maintain the desired crack stress intensity without loss of load due to stress relaxation. Electrical leads may be routed within rather than outside of the sensor to minimize the likelihood of damage or loose parts.

The first in-plant application of the sensor is expected to be an in-core installation in 1986. The objective of that effort is to acquire data on irradiation-assisted stress corrosion cracking in an environment that cannot be simulated outside of the reactor.

STRESS CORROSION MONITORING SYSTEM

A capability for direct measurement of accumulated SCC damage over a period of time suggests application to residual life assessment of susceptible components. Elements of a life prediction methodology are the crack growth response of the sensor, the projected stress changes in the component, and a predictive model that relates crack growth rate to stress, time and temperature. The sensor response is the key information characterizing the effect of the environment at the crack. Loading history at the component, which differs from that at the sensor, could be estimated from existing plant instrumentation or through additional sensors. Reliable predictive models for environmentally-assisted crack growth rates are emerging from a separate line of EPRI-sponsored research at GE.(10) It is expected that this information would be processed by an on-site microcomputer performing the functions of data acquisition, analysis, and display.

Reference 11 describes an operational system for on-line monitoring and life prediction of a fired steam boiler. While different material damage mechanisms predominate in this application, utilization of the data for life prediction and life extension is similar in principle to the stress corrosion monitoring system.

There are both short-term and long-term incentives for the development of such a system. The plant operator receives immediate feedback on the damage consequences of unplanned water chemistry transients, and is able to gage the required time for corrective action to minimize damage. Planned procedures, and particularly those involving off-normal water chemistry, can be tailored or optimized to increase the life of components, reducing repair outage costs. Component life prediction based on in-plant monitoring could also be useful in determining appropriate inspection intervals.

In principle, growth of cracks in defected components can be predicted without the necessity of in-plant measurements. If the material, the environment, the defect, and the stress are defined, and if models are available for relevant material/environment combinations, the crack growth rate and the component life can be predicted.

In practice, the reliability of such predictions is limited by uncertainty in some of the relevant parameters, or in the model itself. A stress corrosion monitor provides a means of confirming the predictive model, or of better characterizing the environmental parameters, under conditions that are well controlled with respect to stress and material variables. The corrosion monitoring system will be useful if resulting component life predictions are sufficiently reliable to support decisions that extend component life or avoid unnecessary outage costs.

## ACKNOWLEDGEMENTS

The research results presented in this report are the work of several investigators at General Electric whose publications are cited. In addition, the authors wish to acknowledge the contributions of Dr. M. G. Benz to the development of the in-plant monitoring and life prediction concept.

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# Severe Accident Containment Integrity

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## Introduction

One aspect of source term consideration is the integrity of reactor containment. The key question to be answered is how will the containment fail given pressure and temperature histories associated with postulated, low probability, degraded core accident scenarios. The failure mode has significant ramifications. If the failure mode were a sudden gross one, such as a sudden rupture of containment wall, large quantities of fission products might be released to the environment immediately with potentially large public health effects. However, if the failure mode were some localized leakage which would lead to containment depressurization, the release of fission product would be gradual and limited and public health effects would be very small. To address this issue, a research program has been undertaken by EPRI at Construction Technologies Laboratories (CTL) and Anatech International, Inc. The former has the testing responsibility, and the latter has the scope of developing an analytical model for failure mode prediction. This paper summarizes the efforts to date with emphasis on experimental findings.

## First-Phase Effort

In the first phase of the testing program, concrete slabs representing segments from reinforced and prestressed containment walls were tested under uniaxial and biaxial tensile loading to understand prestressed and reinforced concrete deformation behavior. The slabs developed discrete cracks and stretched as much as 2%, which is equivalent to an increase in the containment building diameter of about three feet. Also tested in the first phase were segments of steel liner which, in a typical concrete containment, are anchored to the containment inner surface to serve as a leak-tight membrane. The liner plates, containing butt welds or pipe penetrations, withstood up to 6% elongation without rupturing. The first-phase test results are reported in [1]. Data from selected tests in this phase of testing have been used to benchmark the nonlinear, structural ABAQUS-EPGEN computer code [2,3] for prediction of reinforced and prestressed concrete behavior. The objective of the first-phase effort is to provide a basis for checking out the testing facility and analysis code for actual failure characterization to be investigated in Phase 2.

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## Second-Phase Effort

The second phase of testing addresses specifically the concrete containment failure mode characterization under internal overpressurization. A 50-million pound multi-axial reaction rig as shown in Figure 1 was fabricated at CTL for such an investigation. This rig is capable of testing full-scale concrete containment wall segments. As shown in Figure 2, a leakage monitoring fixture was also installed to obtain information on leakage through liner and concrete crack. To date, five tests have been completed.

The preliminary test results of the five completed tests are summarized in [4,5]. All specimens tested except for Specimen 2.5 were of full-scale prestressed concrete containment design with the 1/4-inch prototypical steel liner anchored. Specimen 2.5 had full-scale reinforcement and liner, but the thickness was of half-size because of the constraint of the test rig.

Out of the five completed tests, three are of particular significance. In the following, the important findings of these three tests are highlighted.

## Specimen 2.2

The first significant test is Specimen 2.2 as shown in Figures 3 and 4. The specimen had a weld in the liner plate running in the meridional direction along the center line of the specimen. Continuous light structural angles were used in the meridional direction for the liner plate anchorage. Part of the length (six inches) along the weld seam was not welded. This "manufactured" crack was to act as a "calibrated" leak path to determine air leak rates through the liner and concrete cracks during testing.

During the test, a two-to-one relationship was maintained between the total loading in the hoop direction and the meridional direction. The maximum average hoop strain reached was 1.65%, and the maximum average meridional strain was 0.22%. Rebars in the hoop direction reached 87.2 ksi which is 80% of ultimate, and the ones in the meridional direction reached 65.7 ksi. The maximum liner stress reached was respectively 56.3 ksi hoop and 43.5 ksi meridional which is about 15% above yield. Translating the specimen response into an equivalent typical containment response (assumed to be of 150 feet diameter), it corresponds to an internal pressure of 135 psi with a diameter increase of 2.46 feet.

At the end of the test, it was found that the original six-inch-long liner plate crack extended to a length of nine inches and the crack width opened from zero to three-eighths inch, Figure 5. This liner tearing occurred in a very controlled manner indicating interaction between the liner and concrete. The significance of this controlled liner tearing is that it contradicts the hypothesis of uncontrolled liner rupture. (Without the reinforced concrete behind the liner, the crack would propagate unstably without arrest.) As the crack was opening up, leak rate information was obtained as given in Table 1. Note that beyond the liner plate crack width of 0.1054 inch, no meaningful data was collected because the air supply system was exhausted.

Specimen 2.4

The second significant test is Specimen 2.4, Figures 6 and 7. This was a full-scale prestressed wall segment with a large pipe penetration sleeve located at the center. The penetration was a 36-inch-diameter, 1-inch-thick pipe with a welded connection to the 1/4-inch-thick liner plate. Testing included biaxial load, outward punching shear, and monitoring of air leak-rates. The maximum average hoop strain reached was 2.5%. The maximum applied reinforcement stress in the hoop direction was 95.3 ksi (85% of ultimate) and the liner stress was 63.1 ksi. These stresses correspond to an equivalent internal pressure of 141 psi and diameter increase of 3.75 feet for a 150-foot-diameter containment.

The outward punching shear applied was 897 kips at the time when the maximum average strain in the hoop reinforcing was 2.5%. Up to this point, no measurable air leakage was noted. (Because the liner side was covered as shown in Figure 7, it was not possible to observe liner crack during the test.) However, while the punching shear load was being removed, the pressure chamber behind the liner was completely depressurized implying a liner crack had developed. After disassembling the test fixtures, it was found that indeed a crack of 16 inches at the sleeve-liner plate junction region as shown in Figure 8 had developed. Although the punching shear direction and magnitude applied in this test was somewhat artificial, it showed that the more ductile liner plate could develop cracks prior to the rupture of the reinforcing due to the high concentrated strain developed locally. Consequently, it demonstrated that through liner-concrete interaction in a region of discontinuity, leakage due to liner crack was probable.

Specimen 2.5

The most recent test was Specimen 2.5, Figures 9 and 10, which was a large-scale element representing a junction region of the wall and basemat of a prestressed concrete containment. The purpose of Specimen 2.5 was to further investigate the localized crack of liner as observed in Specimen 2.4. Two possible localized points in Specimen 2.5 were in the discontinuous region of the knee (wall-skirt intersection) and the basemat junction (skirt-basemat intersection).

Loading on the specimen was intended to simulate the bending moment and shear in the containment wall and also the axial load in the wall due to an internal pressurization. Since the test specimen could not produce the exact boundary conditions as they would be in a full-scale containment, analysis was performed to define the loading sequence that would produce the desired behavior corresponding to an internal pressurization of a full-scale containment. Figure 11 shows schematically the loading configuration. For this particular test, no stress was applied to the linear plate or reinforcing in the hoop direction. In the meridional direction, the maximum liner plate strain reached at the wall-skirt junction was 2.25% and the maximum applied stress at the wall end was 44.1 ksi. The maximum reinforcement strain reached was 0.24% and the applied stress was 16 ksi.

At the wall-skirt junction, the liner plate had tended to straighten out across the corner region. In straightening out, due to tension in the liner, the liner plate tended to tear away from the anchorage angles and also tore the backup channel (which ran in the hoop direction) from the concrete. Inspection of the liner after the termination of the test showed four locations at the wall-skirt junction where small liner tears occurred as shown in Figure 12. These small tears were in the discontinuity region where the liner was first joined at the end of the anchorage angles. At the skirt-basemat junction, there was a similar tear at one location where the liner plate joined with an anchorage angle.

These localized failure (tears) at the discontinuities require very high strain concentration. The results of Specimen 2.5 show the potential for liner tearing in a containment at the wall-skirt and skirt-basemat junctions due to an overpressurization.

Discussion

The tests performed to date demonstrate that

1. Unlike steel containment where nonlinearity is due mainly to plastic deformation, the concrete containment invokes nonlinearity (geometric and material) very early into the internal pressurization loading due to concrete cracking and concrete-liner interaction.

2. Liner will develop crack in the discontinuity region if high load strain concentration is induced.

3. Liner crack will not develop if there is no discontinuity to induce high strain concentration in the liner (results of Specimens 2.1 and 2.2 which are not discussed in this paper).

4. Pre-existing crack or crack initiated at the discontinuity region will arrest (instead of propagating in a uncontrolled manner) due to concrete-liner interaction.

5. Liner anchorage appears to play a critical role in concrete-liner interaction.

Consequently, for prestressed concrete containments, it is highly probable that leak due to structural deformation as a consequence of severe core induced overpressurization will develop prior to break.

Post-test correlation analyses are being performed to characterize the liner fracture strain at the discontinuity region. The generic fracture strain calculated can be used in actual containment analysis for local leak location prediction. Also planned are tests of similar specimens with reinforced configurations where reinforcement in the containment wall is much more dense, and the liner anchorage design is slightly different from the prestressed one.

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2. H.D. Hibbitt, B.I. Karlsson, and E.P. Sorensen, "ABAQUS-EPGEN: A General-Purpose Finite Element Code," Vol. 1-4, EPRI NP-2709-CCM, October 1982.

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Table 1

Leak Rate Information at Various Enlargements

of the Crack

| Liner Plate Crack Width In. | Air Leak Rate Pressure psi | Leak Rate Liters/Min. | Leak* % |
|---|---|---|---|
| 0.0058 | 60 | 62 | 0.13 |
| 0.0234 | 50 | 132 | 0.27 |
| 0.0234 | 60 | 155 | 0.32 |
| 0.0234 | 70 | 178 | 0.36 |
| 0.0370 | 50 | 204 | 0.42 |
| 0.0370 | 60 | 227 | 0.46 |
| 0.0370 | 70 | 258 | 0.52 |
| 0.1054 | 50 | 400 | 0.81 |
| 0.1054 | 60 | 530 | 1.08 |
| 0.1054 | 70 | 545 | 1.11 |

*Equivalent leak as % of containment volume per day.

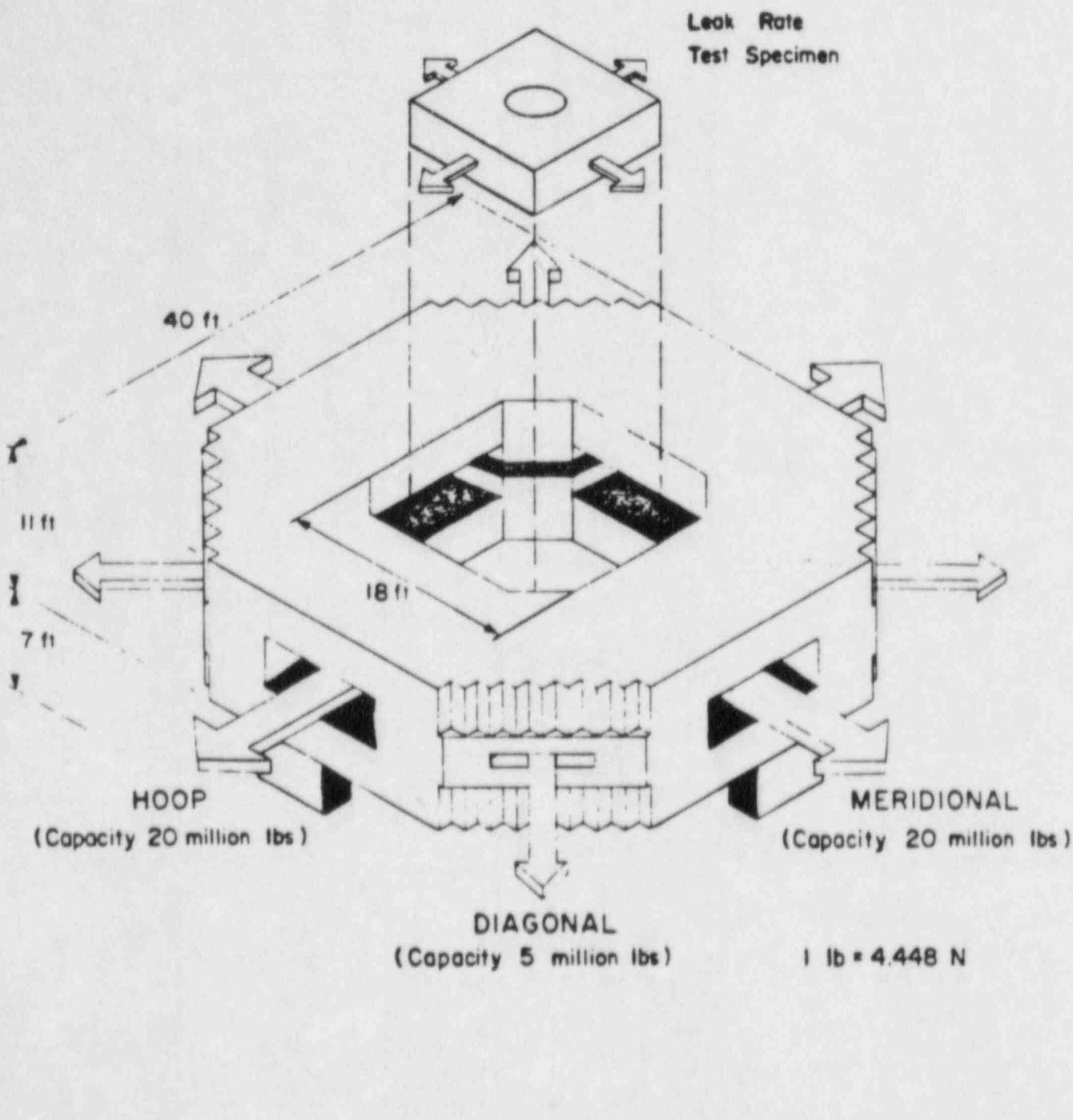


Fig. 1 Biaxial Test Frame Load Capacity

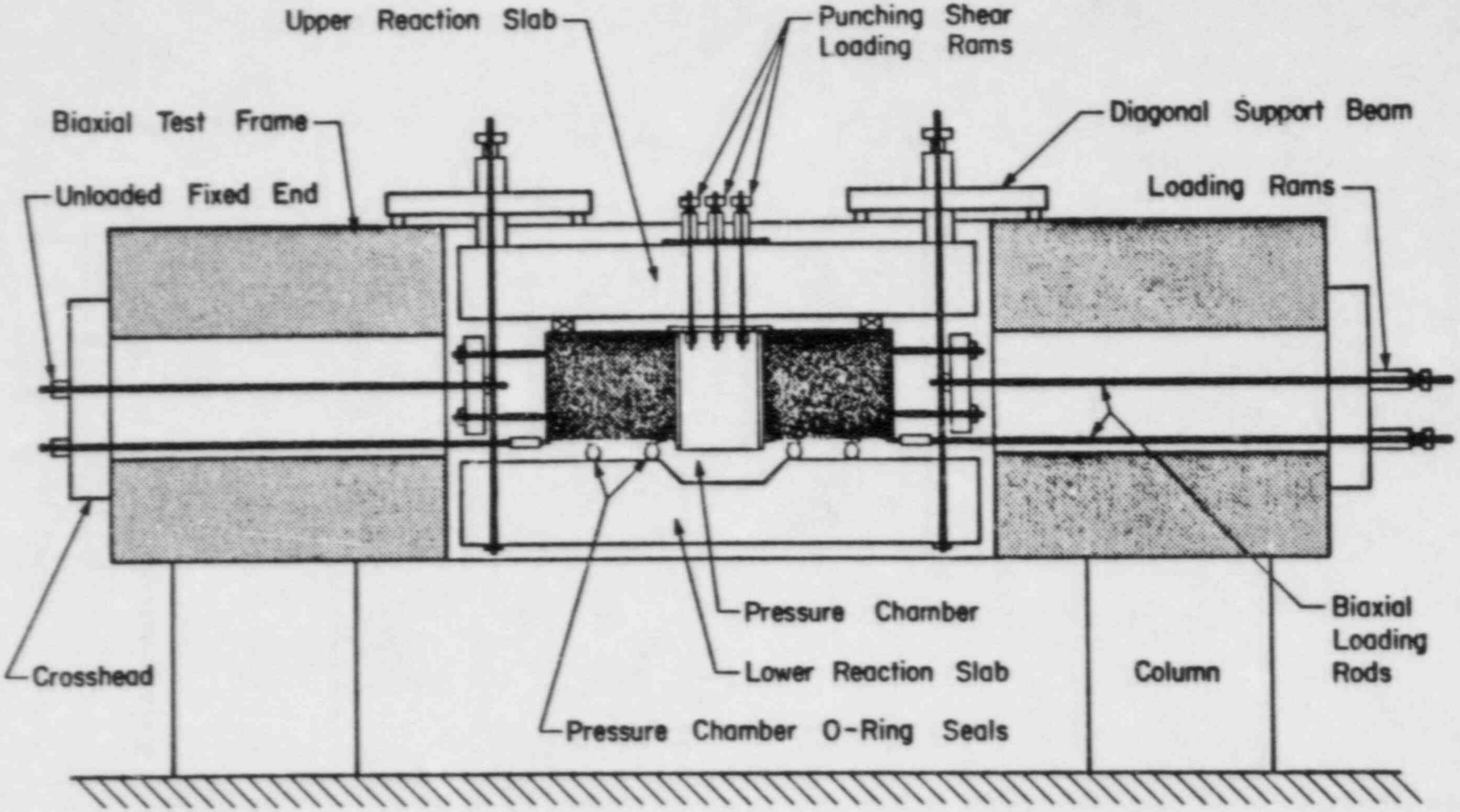


Fig. 2 Cross-Section of Air Leak-Rate Test Fixture and Biaxial Test Frame

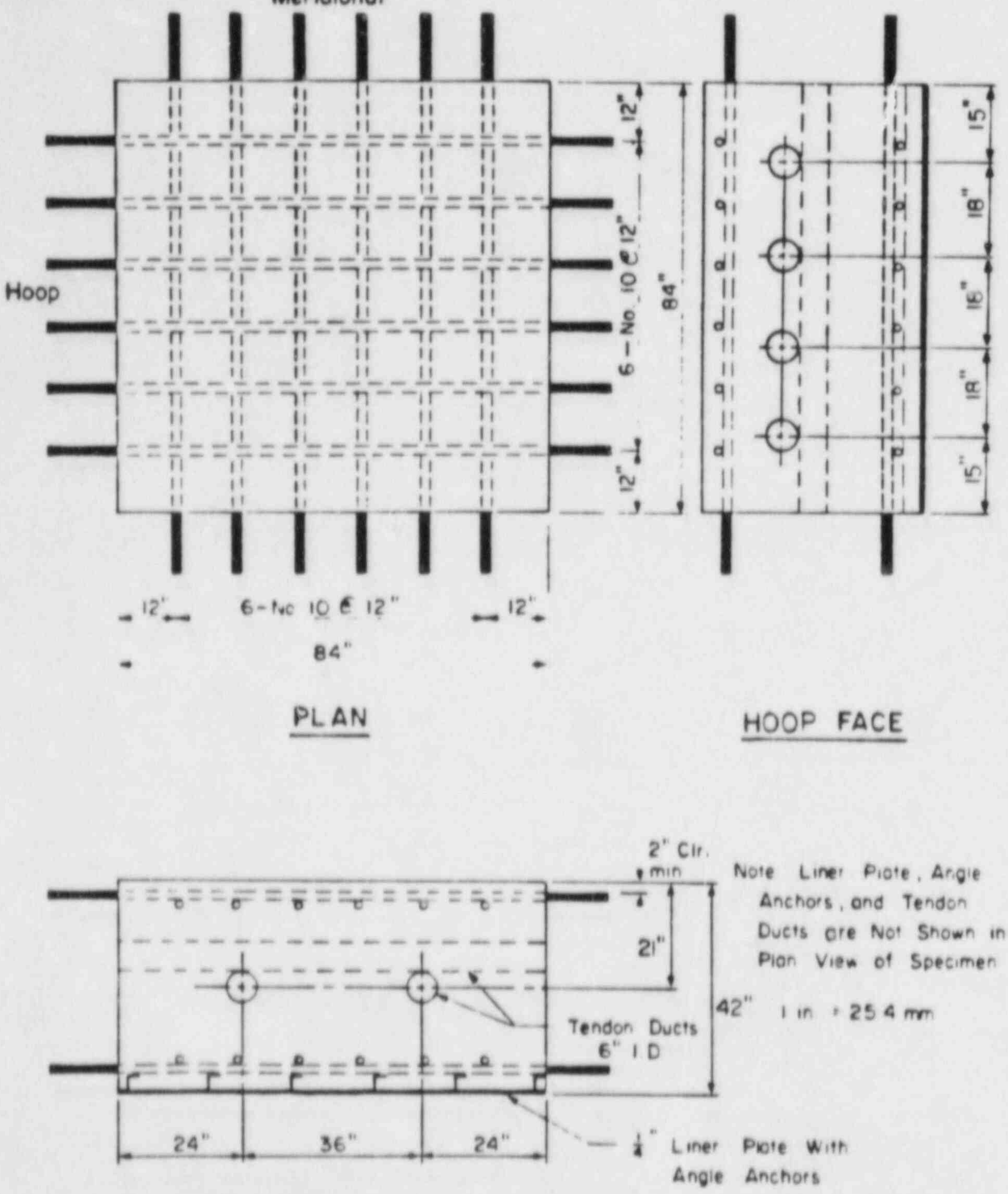


Fig. 3 Reinforcement Details for Specimen 2.2

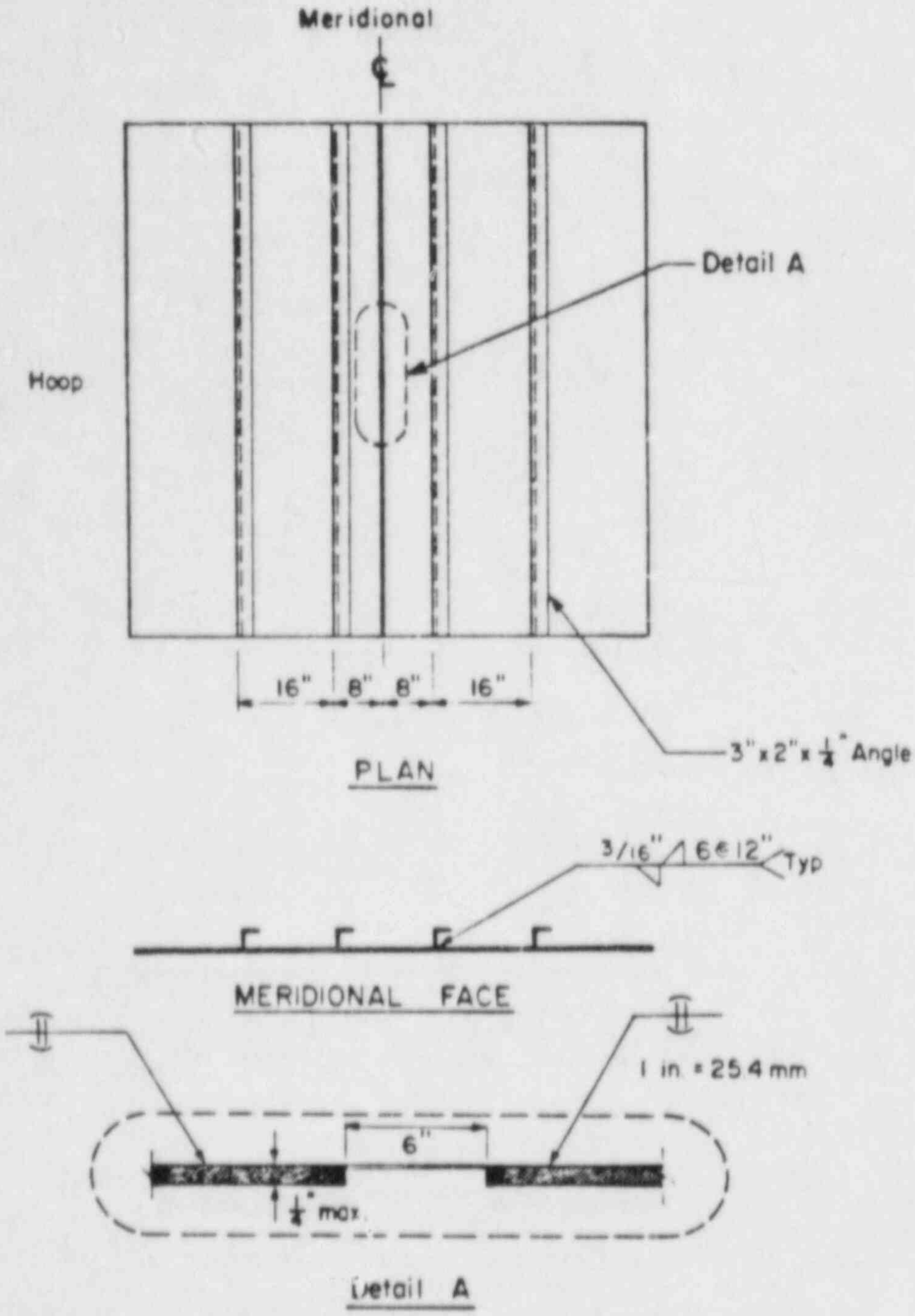


Figure 4 Liner Details for Specimen 2.2

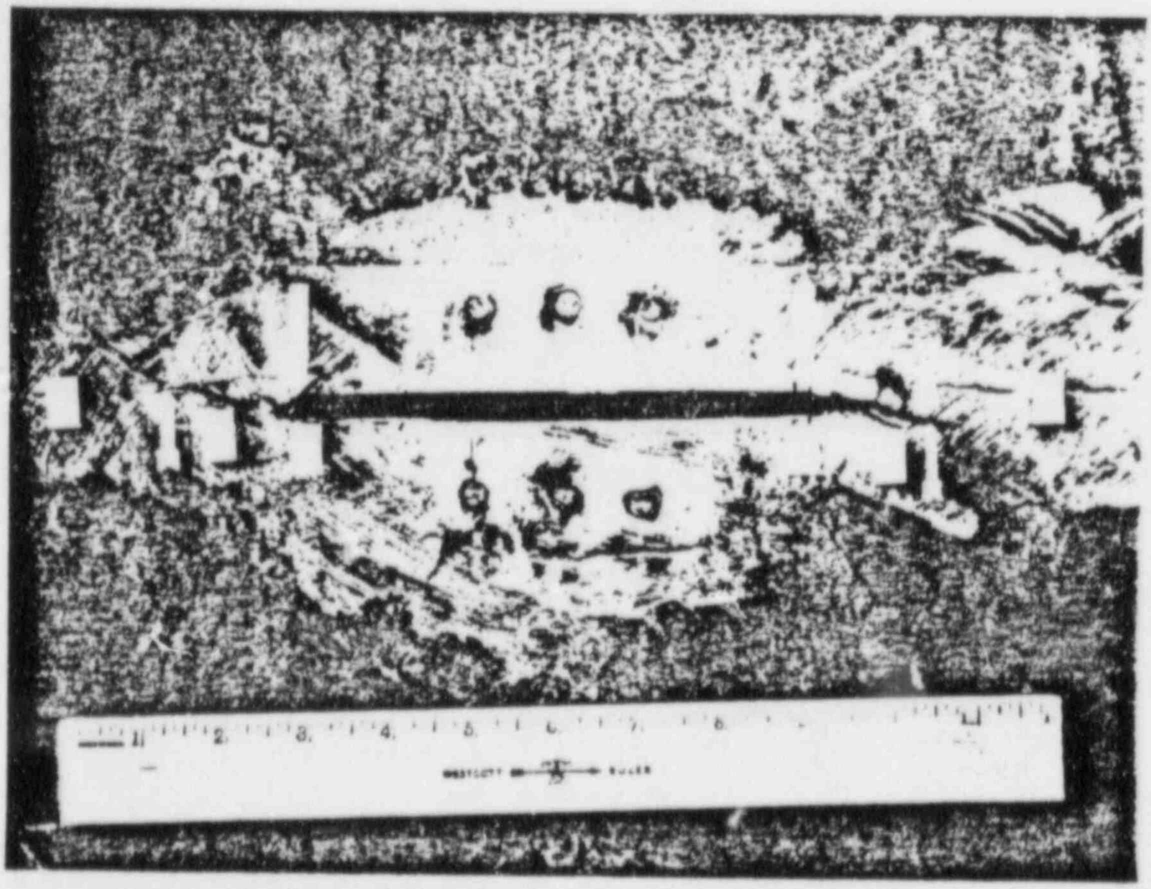

Fig. 5 Crack Configuration for Specimen 2.2

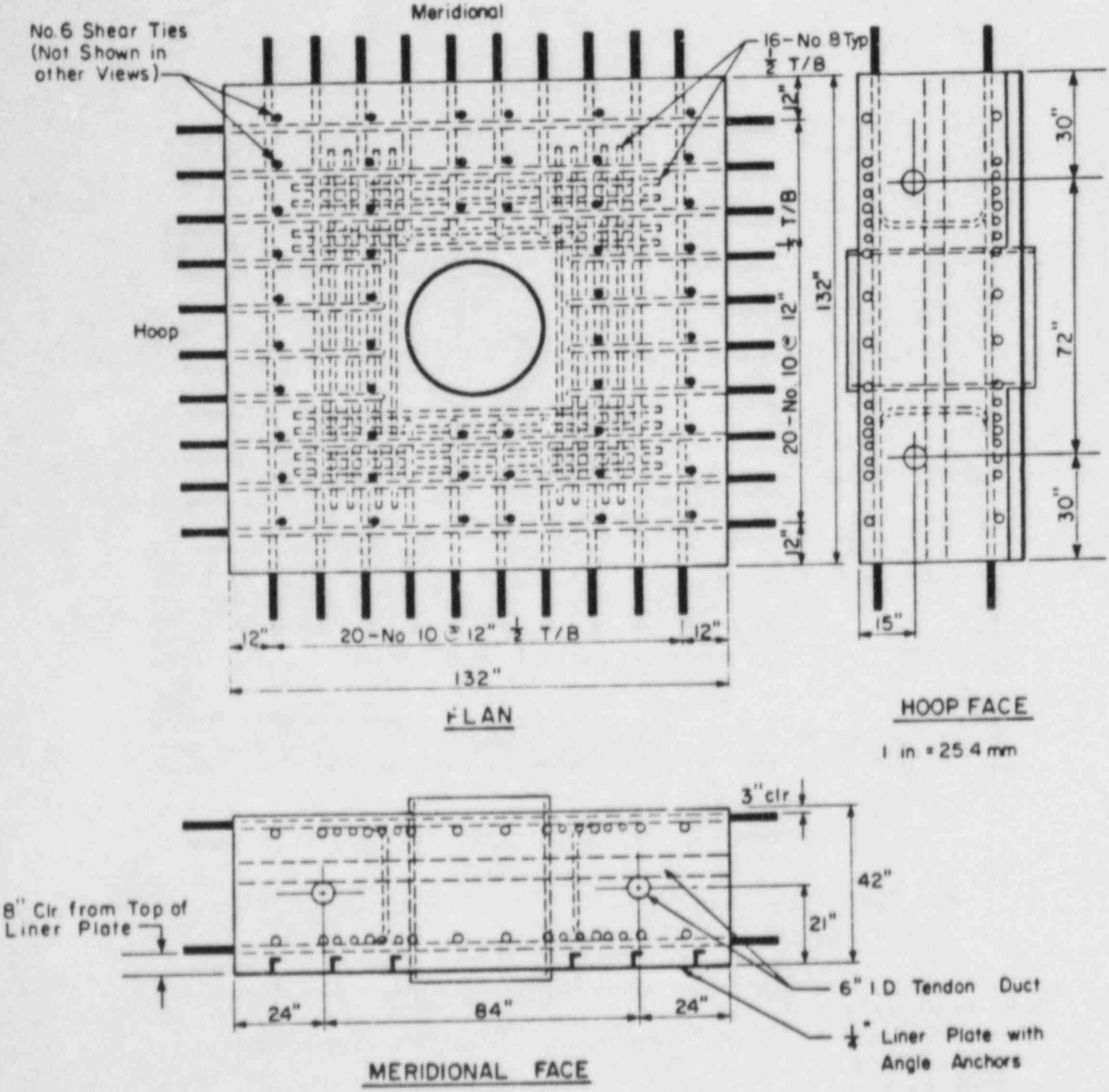


Fig. 6 Reinforcement Details for Specimen 2.4

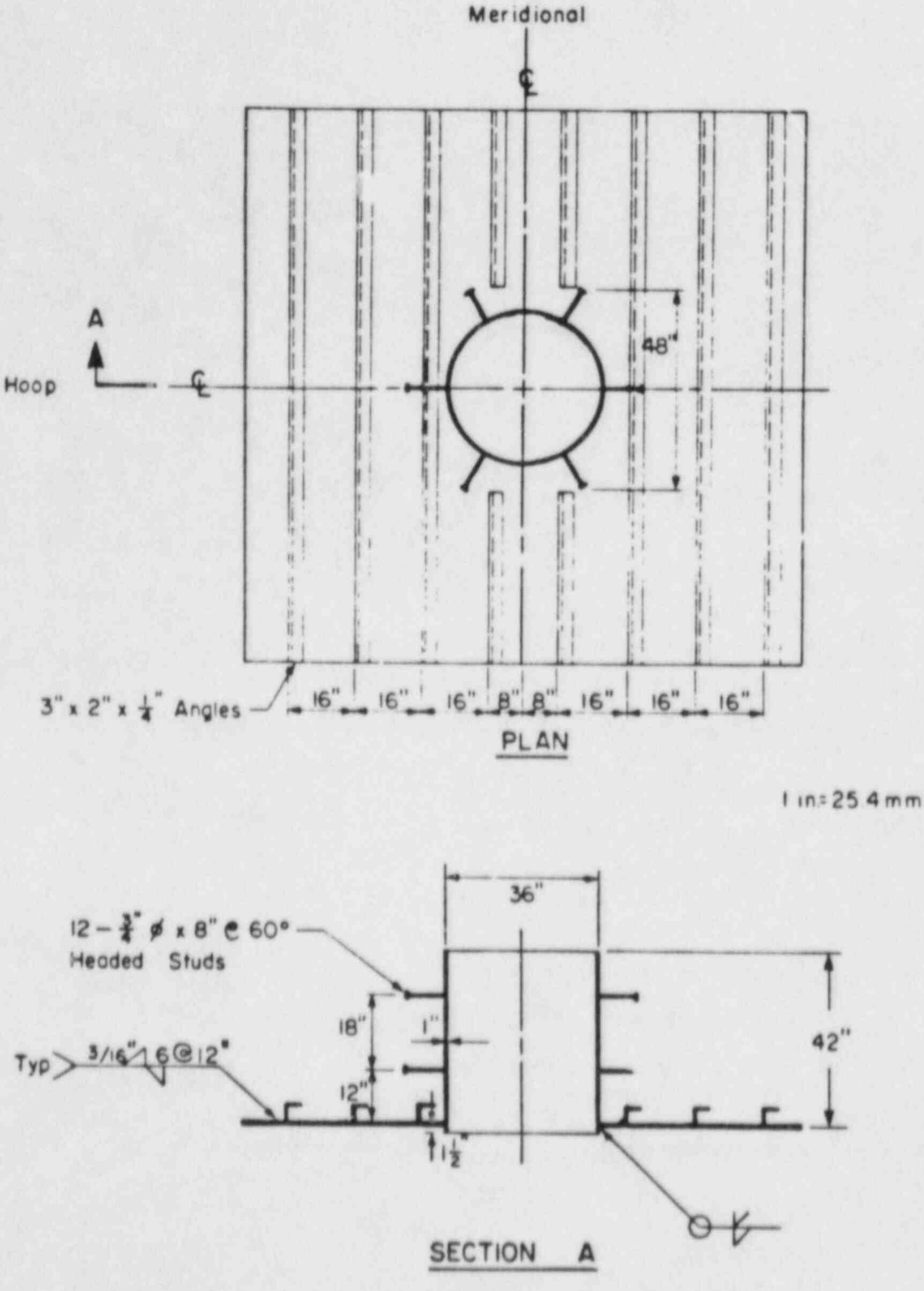


Fig. 7 Reinforcement Details for Specimen 2.4

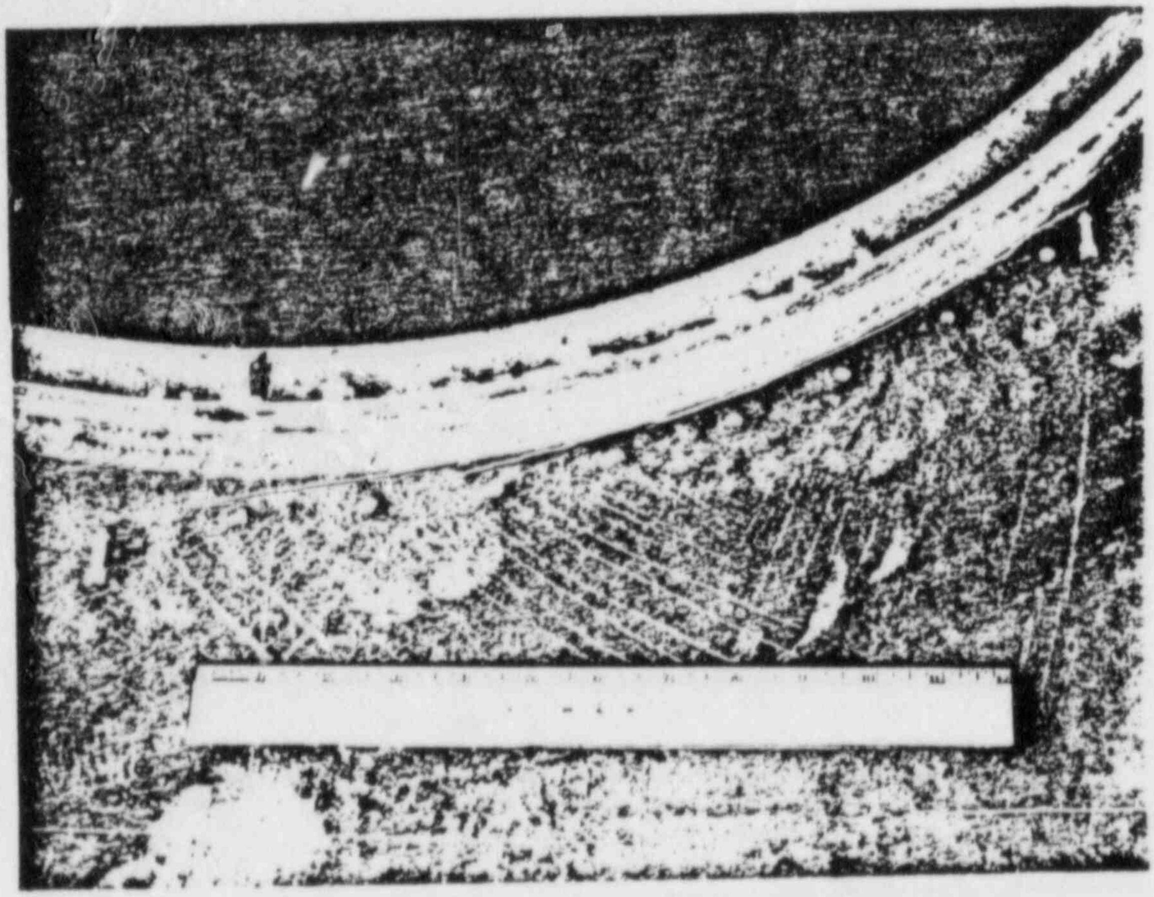

Fig. 8 Crack Configuration for Specimen 2.4

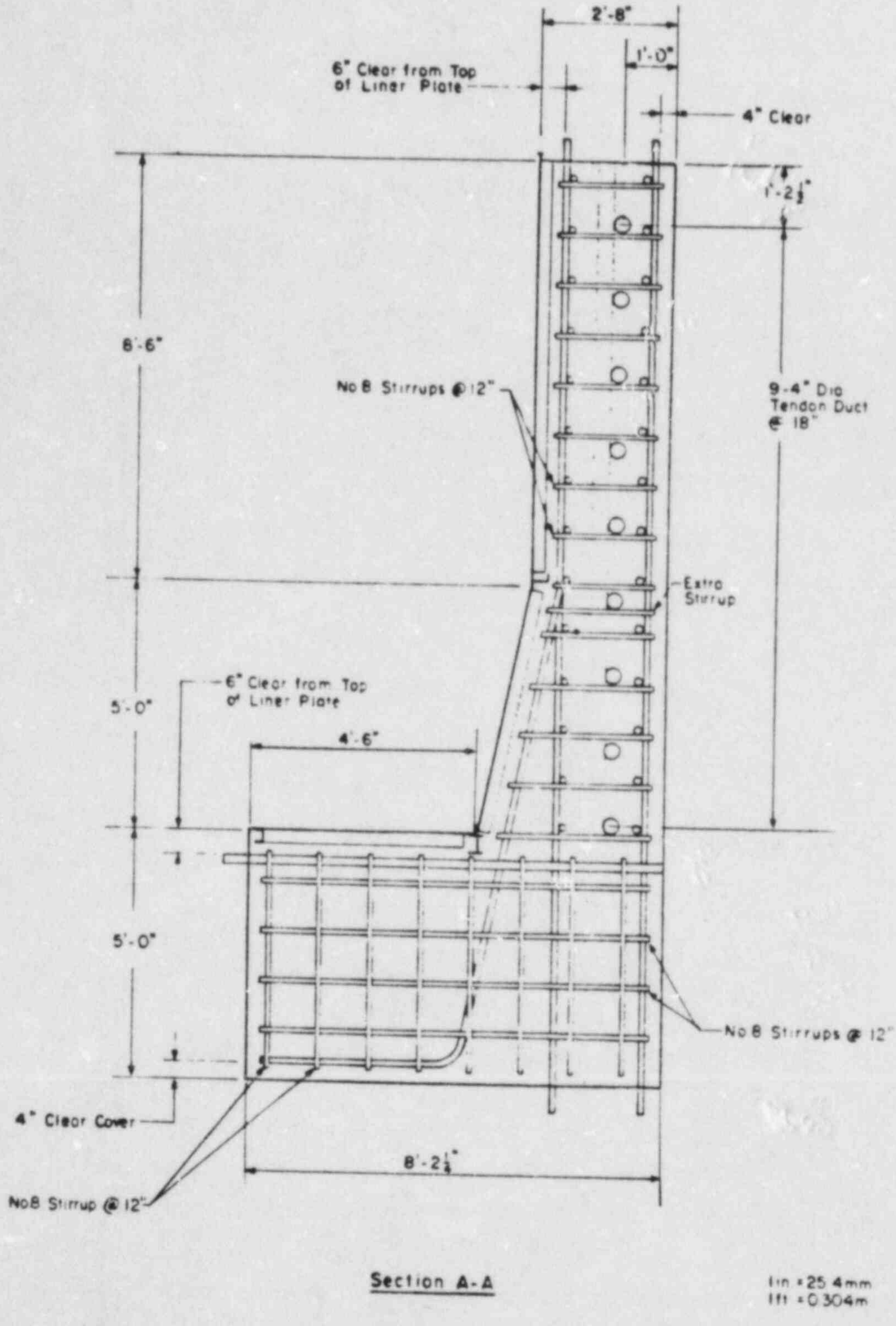


Fig. 9 Reinforcement Details for Specimen 2.5

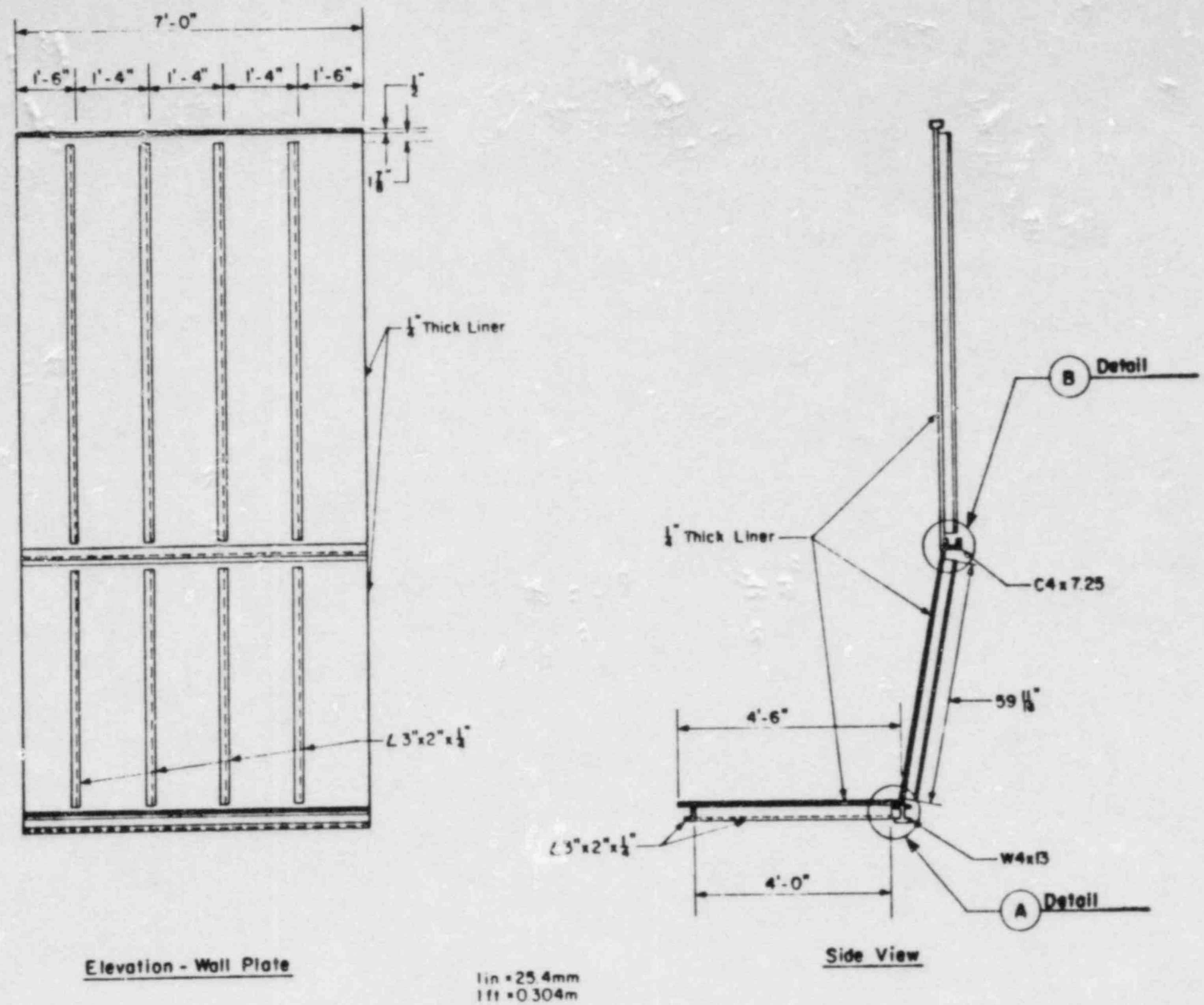


Fig. 10 Liner Details for Specimen 2.5

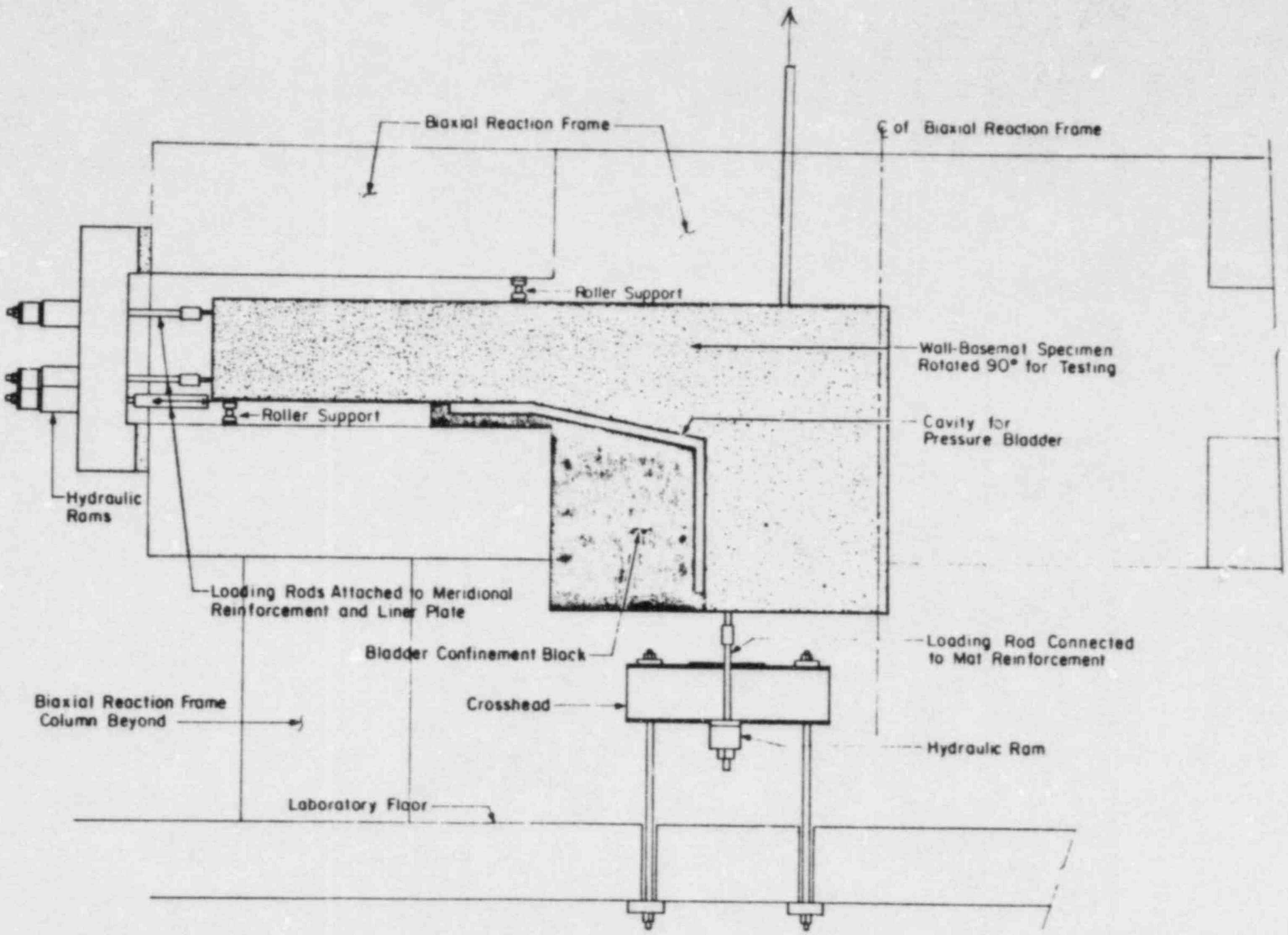


Fig. 11 Loading Configuration for Specimen 2.5

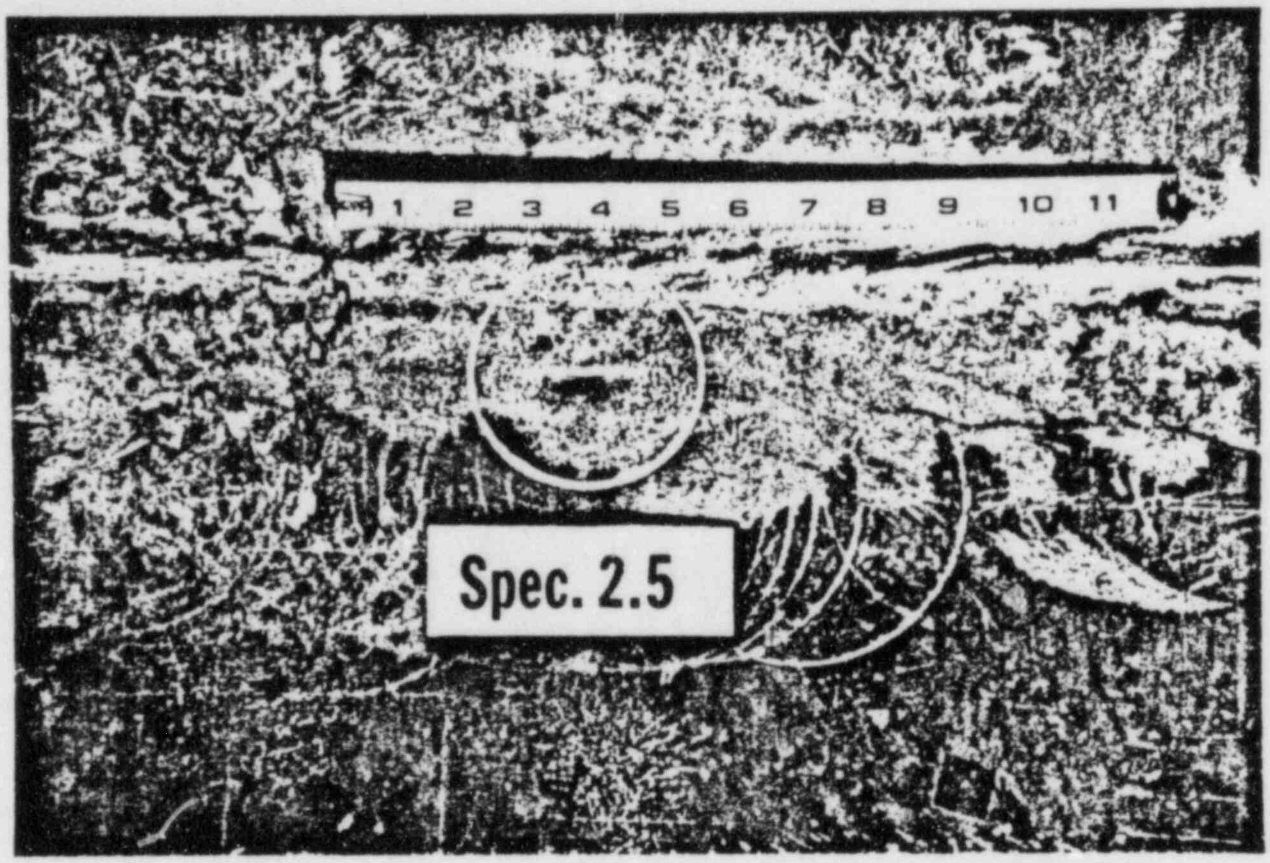


Figure 12. Crack Configuration for Specimen 2.5

# Post APS Source Term Research at EPRI

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## 1.0 Introduction

Several significant occurrences have taken place since the last meeting a year ago. The American Nuclear Society Report has been published (1). In February, 1985 the American Physical Society report (2) became available. A useful product of this report was a series of recommendations for further work. Just recently NUREG-0956 (3) hs been published. Furthermore, the IDCOR reports are now finished and published (4). As a result of the publication of the IDCOR report we now have the fruits of a series of discussion between the NRC and IDCOR in the form of a list of some eighteen issues. These issues have been very helpful in focusing the ongoing source term work. The impact of these many recent "happenings" has been significant and indeed has not yet been completely evaluated and assimilated.

The subject matter of this paper is the EPRI program. This is a sufficiently broad topic that it can not be successfully covered in its complete form. The reader is urged to read the recent paper, "Source Term Research and Prognosis" (5), which covers the EPRI program in more detail than can be done here. The general approach taken for this paper is to emphasize only the following several programs.

## 2.0 Surry Source Term and Consequence Analysis

The objectives of the work on evaluating the results of a degraded core accident in a PWR in a large dry containment such as the Surry reactor were two-fold. Firstly, the project was undertaken to update the radiological source terms for the Reactor Safety Study PWR (6) and to show the effect of the updated source terms on radiological risk projections for the public from severe accidents. Secondly, the project was undertaken to determine the utility of the available methodology for analyzing the outcomes of severe accidents with respect to its technical completeness, its ease or difficulty of application, and its general scrutability or clarity of presentation. This work was carried out by Science Applications International.

The accident sequences analyzed during the work were determined from a re-examination of the Reactor Safety Study classification of dominant accidents sequences for the Surry PWR as given in WASH-1400. This exercise identified $S_2C$ (small LOCA with containment spray injection failure), TMLB' (transient with failure to recover electric power), and V (interfacing LOCA) as the risk dominant sequences that should be reanalyzed. The analytical methods used for the reanalysis consisted of a series of computer codes: MARCH-2 (7) (general thermal hydraulics), PSTAC (8) (primary system thermal hydraulics), TRAPMELT-82 (9) (primary system fission product transport), MATADOR (10) (containment fission product transport), CORCON-MODI (11) (core-concrete interaction), and CRAC-2 (12) (public consequence and risk evaluation). Results of recent experimental programs were used to specify in-vessel (13) and ex-vessel (14) source material release rates and to define best-estimate retention factors for specific auxiliary structures (15) in key accident sequences. Detailed plant data needed for the calculations were provided by Virginia Power Company and staff at the Surry Nuclear Power Station.

The principal findings and conclusions developed during the study were as follows.

(1) The detailed plant data that were collected for the Surry Nuclear Power Station confirmed most of the parameters used in the Reactor Safety Study but they also revealed three significant differences. The data showed that the containment base mat is made of siliceous concrete instead of limestone concrete. Design data also showed that the reactor cavity contains a water-filled neutron shield tank, and analysis suggested the tank would fail immediately after melt-through of the reactor vessel bottom head such that water would drain into the cavity. Finally, plant drawings and elementary calculations confirmed that the safeguards building would remain partially flooded throughout the postulated interfacing LOCA sequence.

The first difference led to important changes in calculated core-concrete interaction rates and gas evolution predictions. The second difference had an impact on the timing and consequence of core debris quenching in the reactor cavity. The third difference introduced the potential for aerosol/water contact which could improve fission product retention by the safeguards building in the V accident sequence.

(2) Thermal hydraulic calculations performed with the MARCH-2 code and with realistic plant performance data indicated that the WASH-1400 $S_2C$ sequence would be a non-core-melt accident. On this basis the sequence was eliminated as a major risk contributor. Its categorization as a key accident sequence in WASH-1400 appears to have been the result of an overly conservative scenario analysis.

(3) Thermal hydraulic calculations performed with the MARCH-2/CORCON code combination and with newer plant data revealed that the TMLB' sequence would likely be a "contained" accident instead of the early containment rupture sequence postulated in WASH-1400. Other recent analyses, which treat natural convection phenomena, also indicated that early venting of the PCS would occur in the sequence. This would reduce threats to containment integrity that would be associated with RPV breach at high pressures. Fission product transport analyses for this case produced small releases because of nominal containment leakage and efficient

deposition processes. Prolonged containment integrity and/or additional fission product removal was expected to preserve the low releases. The resulting source term for TMLB' was more than a factor of 100 below the WASH-1400 estimate.

(4) The accident sequence which essentially determined the risk envelope for the Surry Nuclear Power Station in the present work was the V sequence. Its frequency was calculated to be a factor of 8 less than in WASH-1400, on the basis of testing procedures which had been introduced since completion of the Reactor Safety Study. In addition, deposition of fission product aerosols on surfaces along the transport path to the safeguards building and capture by the water pool or by surfaces in the safeguards building were calculated to result in a source term which was a factor of 10 less than the corresponding WASH-1400 assignment for the sequence.

(5) The public risk represented by the updated source terms was assessed with an improved version of the computer code (CRAC) than was used in the Reactor Safety Study. The site population and meteorological data used in the calculations also came from Reactor Safety Study files, as did the assumptions and rates regarding evacuation and relocation. The resulting analysis predicted no early fatalities for the updated source terms, and the latent fatality (cancer) risk was calculated to be about a factor of 20 lower than produced by the corresponding Reactor Safety Study source terms.

(6) The analytical methodology used to generate the new results was found to be clearly superior to the techniques used in the earlier Reactor Safety Study, and this permitted a more realistic analysis of accident processes and fission product behavior. However, the methodology was cumbersome and expensive to apply because of the number of manual interfaces between computer codes, the various modeling or programming errors that required correction, and the overall problem execution times that were encountered. Furthermore, instances arose where the methodology contained either inadequate or no models of phenomena/processes that were considered important to the source term calculations. In these cases the results of external analytical and experimental programs were utilized to provide alternate models or to guide limiting assumptions. Limited sensitivity calculations were also performed to evaluate the impact of key parameters and these results were factored into the definition of the best-estimate (updated) source terms.

A complete discussion of the contents of this study is contained in EPRI Report NP-4096 (8).

## 3.0 Recent STEP Results

The objective of the source term experimental program (STEP) is to conduct in-reactor experiments on the behavior of volatile fission products for conditions representative of risk dominant LWR severe accidents sequences (16-18). The data will be used to test and validate assumptions and models of fission product release, transport, and deposition in such accidents. The experiments will also yield supporting data about the timing and extent of hydrogen generation, the timing of fuel degradation, the amount of volatile fission product release, and the behavior of control rods during severe accidents. The work is being conducted at Argonne National Laboratory and receives major support from EPRI and support from DOE, NRC, Ontario Hydro and Belgo-Nuclaire.

Four tests were carried out in an in-pile test unit which was inserted into the TREAT reactor. The in-pile vehicle comprises a bundle of four irradiated (burnup of 30,000 to 40,000 MWd/t) fuel rods mounted inside zirconia flow and insulator tubes and contained in a high-temperature alloy pressure tube. Steam generated externally enters the bottom of the pressure tube and exits at the top, after having traversed the test fuel and a sample collection region.

The sample collection region contains a sample tree upon which are mounted various types of deposition coupons at three elevations, the post-test examination of which is used to gather information on fission product chemistry and to measure aerosol characteristics. A hydrogen monitor is also included to study the Zircaloy water reaction. A port near the bottom and another near the top of the sample collection region each draw a side stream of 6% of the total flow from the flowing steam into an aerosol characterization system containing labyrinthine sedimentation chambers, fine wire impactors, and microweave metal filters. Each system incorporates three separate flow paths that can be operated independently by external flow control valves to allow limited temporal resolution. The metal filters are scanned by radiation detection instruments during and after the test. Thermocouples are located both internal and external to the vehicle, which is mounted inside a secondary can. The vehicle is kept above the steam saturation temperature during the test by means of heater tapes (nominal temperature of 644K). It is designed so that fuel loading and post-test disassembly and sample recovery may be accomplished remotely.

The first STEP test was a simulation of a PWR unterminated large-break LOCA (WASH-1400: AD). This test was run at a pressure of 45 psia (0.31 MPa), with steam velocity varying from 5.5 to 1.9 lb/h (0.69-0.24 g/s) and a temperature ramp rate of 5.4° F/s (3 K/s). The second STEP test was a simulation of a BWR feedwater failure transient with the concomitant failure of core makeup water systems and residual heat removal systems (WASH-1400: TQUW). This test was run at a pressure of 25 psia (0.17 MPa), with stem velocity varying from 2.7 to 1.8 lb/h (0.34-0.23 g/s) and a temperature ramp rate of 4.3° F/s (2.4 K/s). The third test simulated the loss of all ac power and of reactor coolant system heat removal in a PWR (WASH-1400: TMLB'), and the fourth was a TMLB' simulation with silver alloy control rods in place. These last two tests were run at 1130 psia (7.8 MPa), with steam velocity varying from 4.3 to 1.9 lb/h (0.54-0.24 g/s) and a planned temperature ramp rate of 1° F/s (0.6 K/s).

All tests performed satisfactorily and the effort is now focused on the processing and analysis of data recorded during the tests and on the recovery, instrumental analysis, and interpretation of the fission product and aerosol characterization samples that were present in each experiment. Hydrogen and fission products were released in all tests. The hydrogen yields generally agree with predictions of the more popular models. The relative fission product activity measured in the sample collection region of the in-pile test units varied between tests. The levels were similar for STEP-1 and STEP-2. However the levels for STEP-3 and STEP-4 were lower by roughly an order of magnitude. Gamma scans of the sample tree as well as the aerosol canister effluent filters indicated the same result. Cesium radionuclides were detected in all four tests, iodine radionuclides were detected in the gamma scans from only STEP-1 and STEP-2, and tellurium radionuclides were identified only in the gamma scans from STEP-1.

The lower activity levels in STEP-3 and STEP-4 are considered to be the result of several factors. First, the lower gas flow velocities (<1 cm/sec) in these experiments should have caused less transport of radionuclides through the vehicle. Second the lower ultimate fuel temperatures and reduced time at temperature for the experiments should have caused a smaller release of fission products from the fuel. Actually, the fuel temperatures in STEP-3 and STEP-4 were even lower than anticipated before the tests while the temperature of the gas and structures above the fuel zone were higher than anticipated. It has been postulated that this was the result of thermal convection which developed in this region of the vehicle under the low forced-flow conditions that existed in the two tests.

Preliminary observations obtained from limited SEM, microprobe, and particle characterization analyses that have been done on STEP-1 samples to date indicate the following. Considerable amounts of tin were identified on deposition samples. In addition, several sample sites indicated co-deposition of cesium with molybdenum, and cesium with iodine. In some cases silver has also been identified as a component with cesium and iodine. The results of the initial particle counting analyses indicate that the distribution function obtained from particles sampled from the lower canister (open during the last half of the test) are in rough agreement (within one order of magnitude of ten) with the distribution function obtained from particles sampled from the upper canister (open during the first half of the test) in the particle diameter range between 0.1 and 1.0 μm. Furthermore there is good agreement between the data taken from different areas and wires. The maximum in each of these distributions lies at approximately 0.4 μm. These results support the conclusion that for STEP-1 a large population (with particle concentrations on the order of $10^6$ p/cm$^3$) of submicrometer particles was formed, and that this comprised the majority of particles that entered either canister port during either half of the test.

Work is continuing on the post-test examination of data and samples from all four STEP tests.

## 4.0 Effects of Thermal Convection in Postulated High Pressure Accidents in PWRs

Consequences of the postulated PWR high pressure scenarios e.g., TMLB' and $S_2D$ (small LOCA with failure of ECC injection system), have been evaluated with the NRC code MARCH (19) and the IDCOR code MAAP (20) for the core heat-up and degradation calculation. These codes mainly employ the "once-through" forced convection flow modeling, in which the steam and hydrogen generated flow through the core, PWR vessel, piping and components, in a uni-directional fashion. The MAAP code transports the fission products generated during the core heat-up and deposits them on the piping and the components, which increase in temperature due to the fission product self-heating. A natural convection flow model has recently been introduced in the MAAP code.

The "once-through" convective flows may not be valid during the core heat-up and degradation phase, when there is a radial variation of the steam heat-up and hydrogen generation in the core. Buoyancy-driven natural convective flows develop between the core and the upper plenum, the upper plenum and the dome, and the upper plenum and the rest of the primary system loops. These flows distribute the heat generated in the core to the upper parts of the PWR vessel and the primary coolant system. These flows, therefore, affect (a) the magnitude and the rate of hydrogen generation, (b) the elapsed time before start of core melting, (c) the transport of fission products in the PWR primary coolant system (PCS), (d) the temperature of the PCS piping and components, (e) the revaporization of the fission products from the PCS surfaces, and, lastly, and of particular importance, (f) the course of high pressure accidents.

Inclusion of natural convection flow modeling, and determination of its effect on the parameters mentioned above, is the objective of the recent analytical work with the CORMLT code (21-23) and of the recent experimental work at Westinghouse. Of particular significance is the temperature history of the PWR PCS to assess if, for the risk-dominant high pressure scenarios, a local failure in the PCS may occur before the postulated vessel melt-through and radically alter the consequences of such accidents.

Some initial results for the temperature histories of PCS piping and pressurizer gas were obtained from the CORMLT code. A representative result is shown in Figure 1. It is seen that the temperatures of surge-line piping reaches very high values. If attention is directed to times prior to core degradation (say, ~3000s) for which geometrical complications in flow modeling have yet to occur, it is seen that pipe-wall temperatures are still so high (approaching 1260K (1800°F)) as to question mechanical survivability. (At 1800°F, the yield stress of steel is less than 20% of its value at normal operating temperatures.) The gas-discharge temperatures from the pressurizer are also very high, and at these temperatures it is doubtful that the (spring-loaded) safety valves will operate as designed.

Experimental benchmarking work at Westinghouse R&D Laboratories involves simulated accident conditions in a 1/7th linear-scale-replica of a Westinghouse PWR core, upper plenum, piping and steam generators. Experiments using low pressure water and $SF_6$ gas have been performed. Visual observations of flow patterns and motion picture records were obtained and core heater watt-meter data were digitally recorded and stored at selected intervals. A two-color, laser-doppler anemometer system with traversing optics was used to obtain velocities in the front row of fuel assemblies, and in line with or in

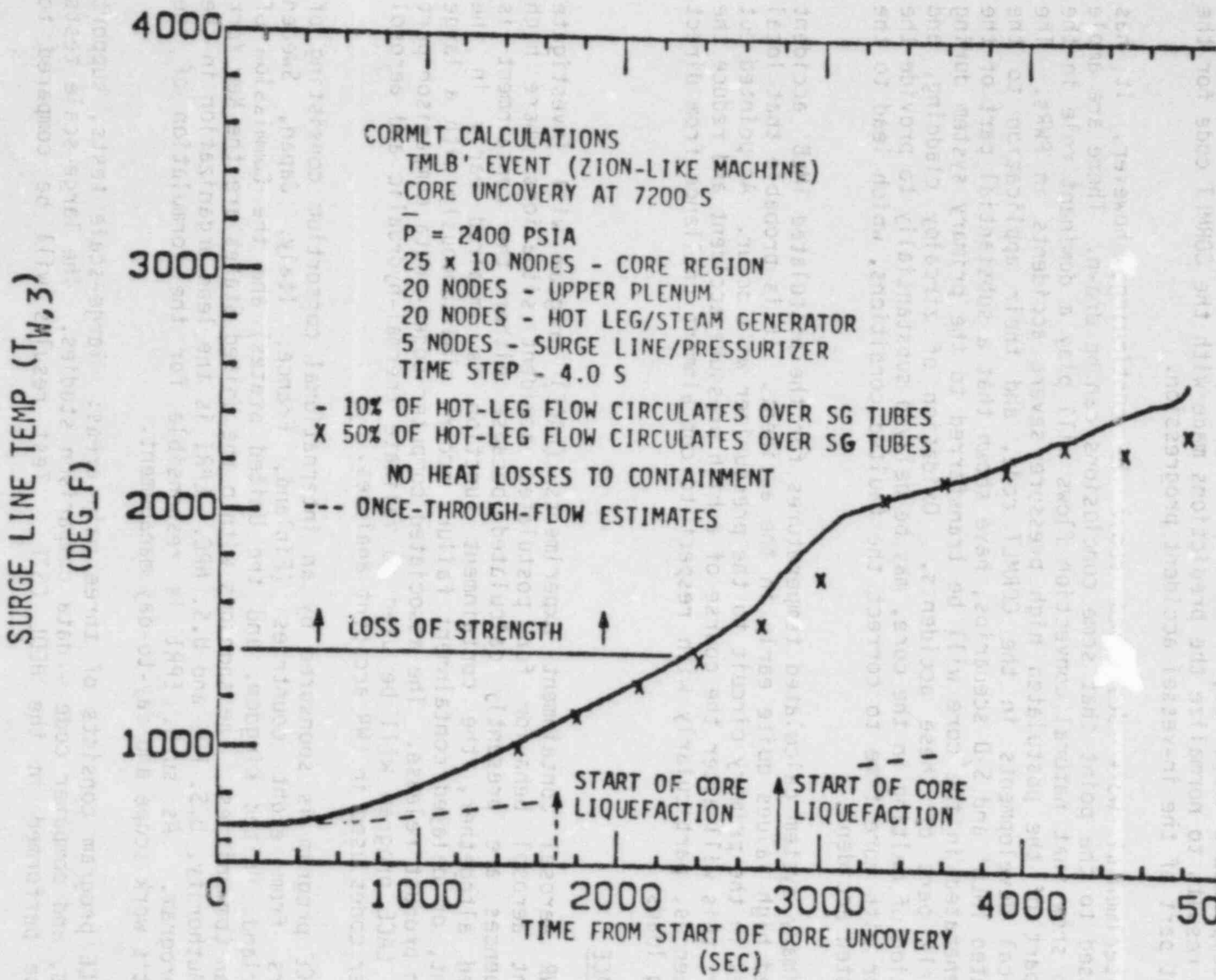


Figure 1. EFFECTS OF PCS FLOW OPTIONS ON THERMAL THREATS TO PCS BOUNDARY

the behind the front row of guide tubes, and support tubes in the upper plenum (Figure 2).

Argonne National Laboratory uses the COMMIX-1A code (24), a three-dimensional fluid flow code, to analyze and pre-predict the data measured in the Westinghouse tests. The ultimate objective is to obtain benchmark predictions of the temperature histories, in a few prototypic postulated accidents, and to use the results to normalize the predictions made with the CORMLT code for the relevant part of the in-vessel accident progression.

The experimental work described above is continuing; however, it has progressed to the point that some conclusions can be drawn. There are ample data to show that natural convection flows will play a dominant role in the early part of the postulated high pressure severe accidents in PWRs. The analytical developments in the CORMLT code, and their application to the postulated TMLB' and $S_2D$ scenarios, have shown that a substantial part of the heat generated in the core will be transferred to the primary system during the early part of these accidents. Oxidation of zircaloy cladding, and initiation of melting in the core, may be delayed substantially to provide the operator with more time to correct the faulted conditions, which lead to the postulated accident.

The primary system calculated temperatures for the postulated TMLB' accident achieved high values quite early in the accident. It is probable that local failures in the primary circuit to the pressurizer will occur. As pointed out earlier, this will alter the course of a high pressure accident and reduce the consequences, particularly with respect to containment challenge from direct heating loads.

## 5.0 LACE

The LWR Aerosol Containment Experiments (LACE) program will investigate inherent aerosol behavior for postulated accident situations where high consequences are presently calculated because either the containment is bypassed altogether, the containment function is impaired early in the accident, or delayed containment failure occurs simultaneously with a large fission product release. The associated computer code - data comparison part of the LACE program will be used to validate thermal-hydrualic and aerosol computer codes used in LWR accident analyses.

The LACE program is sponsored by an international consortium consisting of sponsors from eight countries (Finland, France, Italy, Japan, Sweden Switzerland, United Kingdom, and the United States) and the Commission of European Communities. The sponsors within the United States are the New York Power Authority, U.S. DOE and U.S. NRC. EPRI is the lead organization in the LACE program. As such, EPRI is responsible for the formulation of the project's work scope and day-to-day management.

The LACE program consists of three main areas: large-scale tests, support studies, and computer code - data comparison studies. The large-scale tests will be performed in the HEDL CSTF. Test results will be compared to

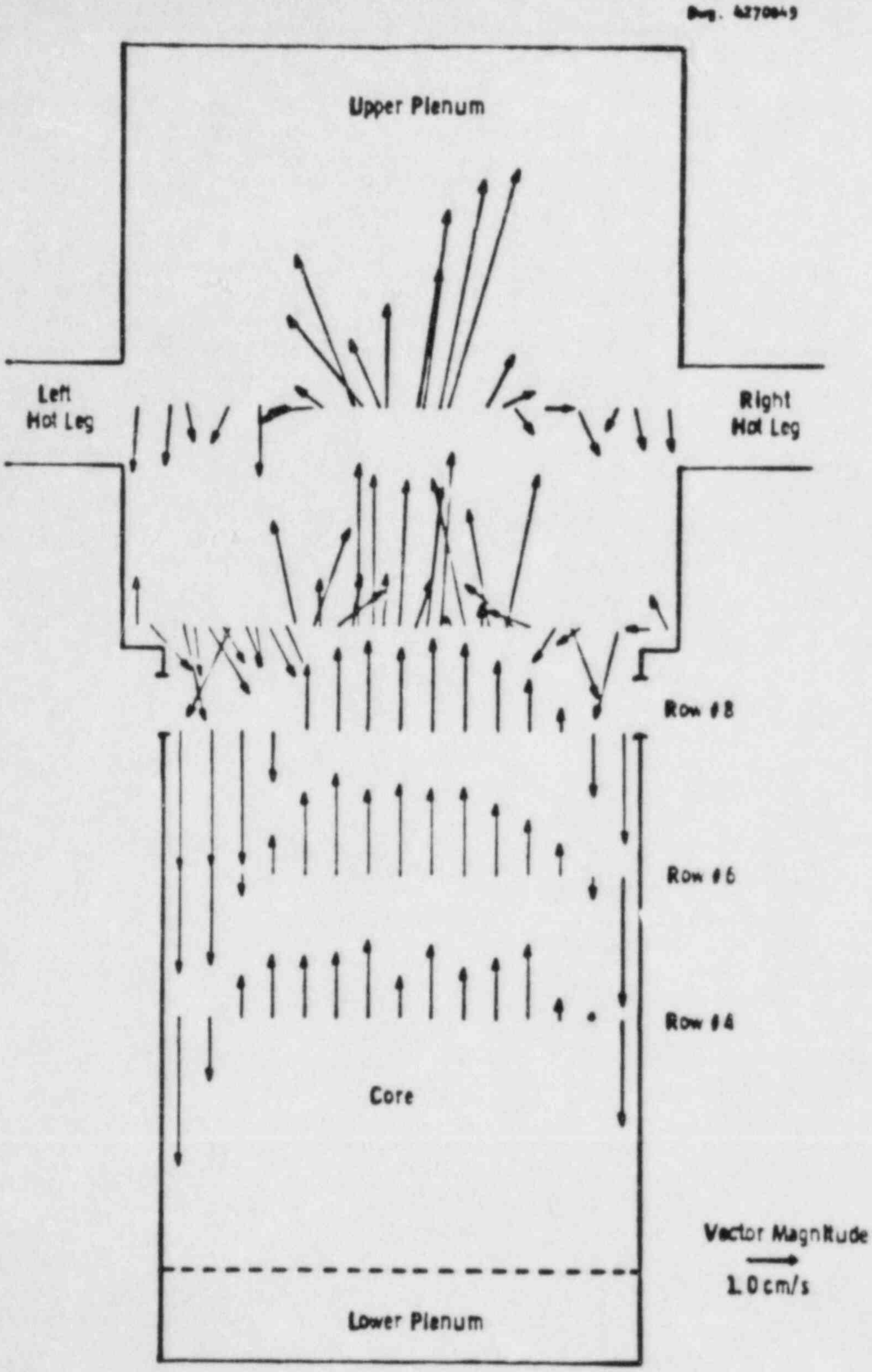


Figure 2. Velocity vectors with 28 KW input into water and with steam generator connected.

applicable aerosol and thermal-hydraulic computer codes under controlled conditions. Both pre-test and post-test computer code comparisons with test data will be completed. Each member of the consortium will also provide indirect support to the program either in terms of small-scale supporting tests, computer calculations, or on-site manpower.

The initial containment bypass scoping tests have been completed, data analyzed, and a draft report written. Six large-scale LACE tests have been defined with test objectives and conditions. Test plans have been completed for the first three LACE tests. Preparations and facility modifications for the first LACE test have been completed, and present efforts are directed towards development of a suitable insoluble aerosol source.

## 6.0 Concluding Comments

In the preceeding paragraphs several important EPRI programs are discussed along with discussions of programs supported by other organizations in addition to EPRI. It is our belief that very significant progress has been made in developing an understanding of serious reactor accidents and that this understanding has now reached a point permitting regulatory decision.

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