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Proceedings of the U.S. Nuclear Regulatory Commission

Twenty-Fourth Water Reactor Safety Information Meeting

Volume 1

- Plenary Session
- High Burnup Fuel
- Containment and Structural Aging

Held at
Bethesda Marriott Hotel
Bethesda, Maryland
October 21-23, 1996

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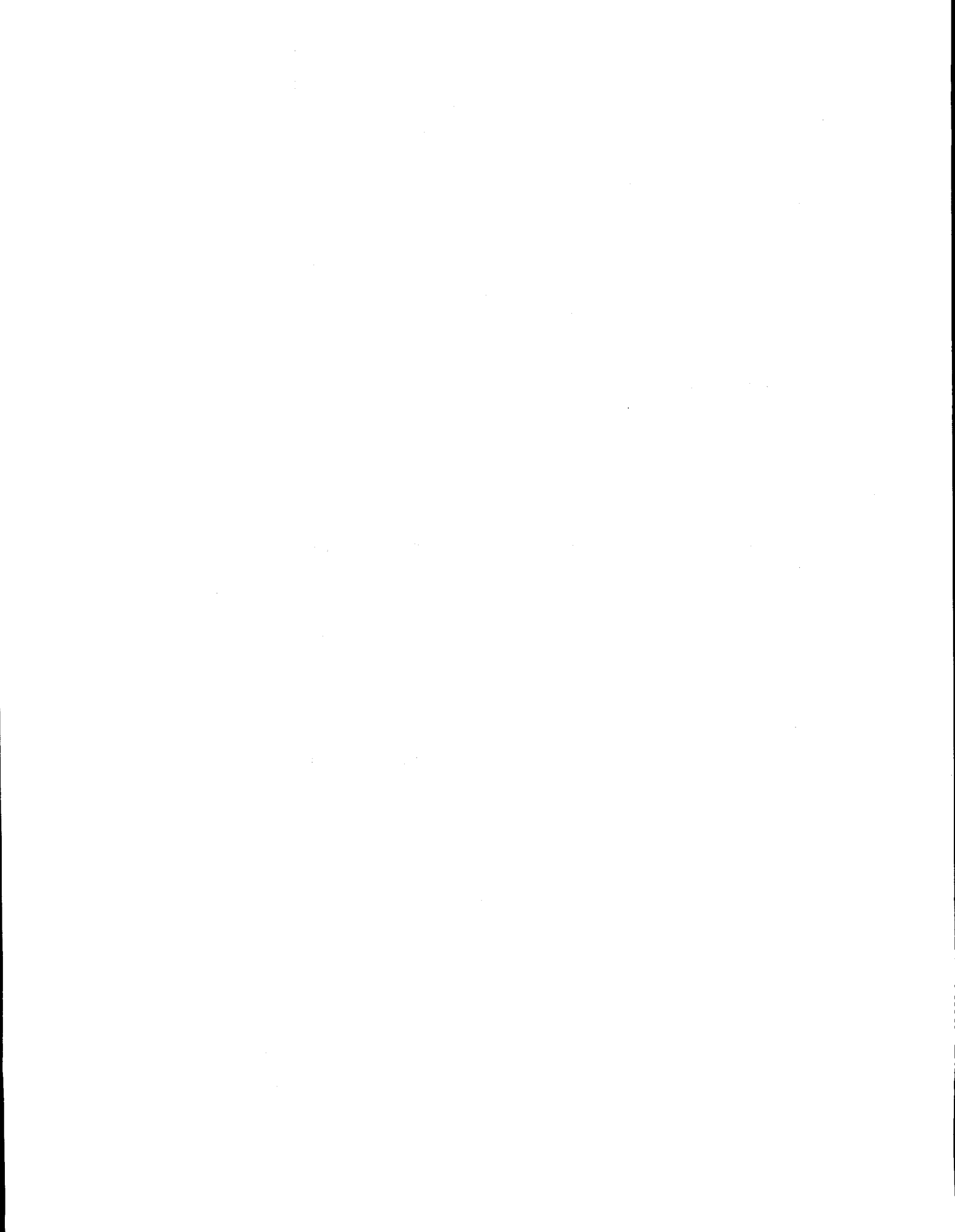
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ABSTRACT

This three-volume report contains papers presented at the Twenty-Fourth Water Reactor Safety Information Meeting held at the Bethesda Marriott Hotel, Bethesda, Maryland, October 21-23, 1996. 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 papers presented by researchers from Czech Republic, Finland, France, Japan, Norway, Russia and United Kingdom. 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
24TH WATER REACTOR SAFETY INFORMATION MEETING**

OCTOBER 21-23, 1996

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**PROCEEDINGS OF THE
24TH WATER REACTOR SAFETY INFORMATION MEETING
OCTOBER 21-23, 1996**

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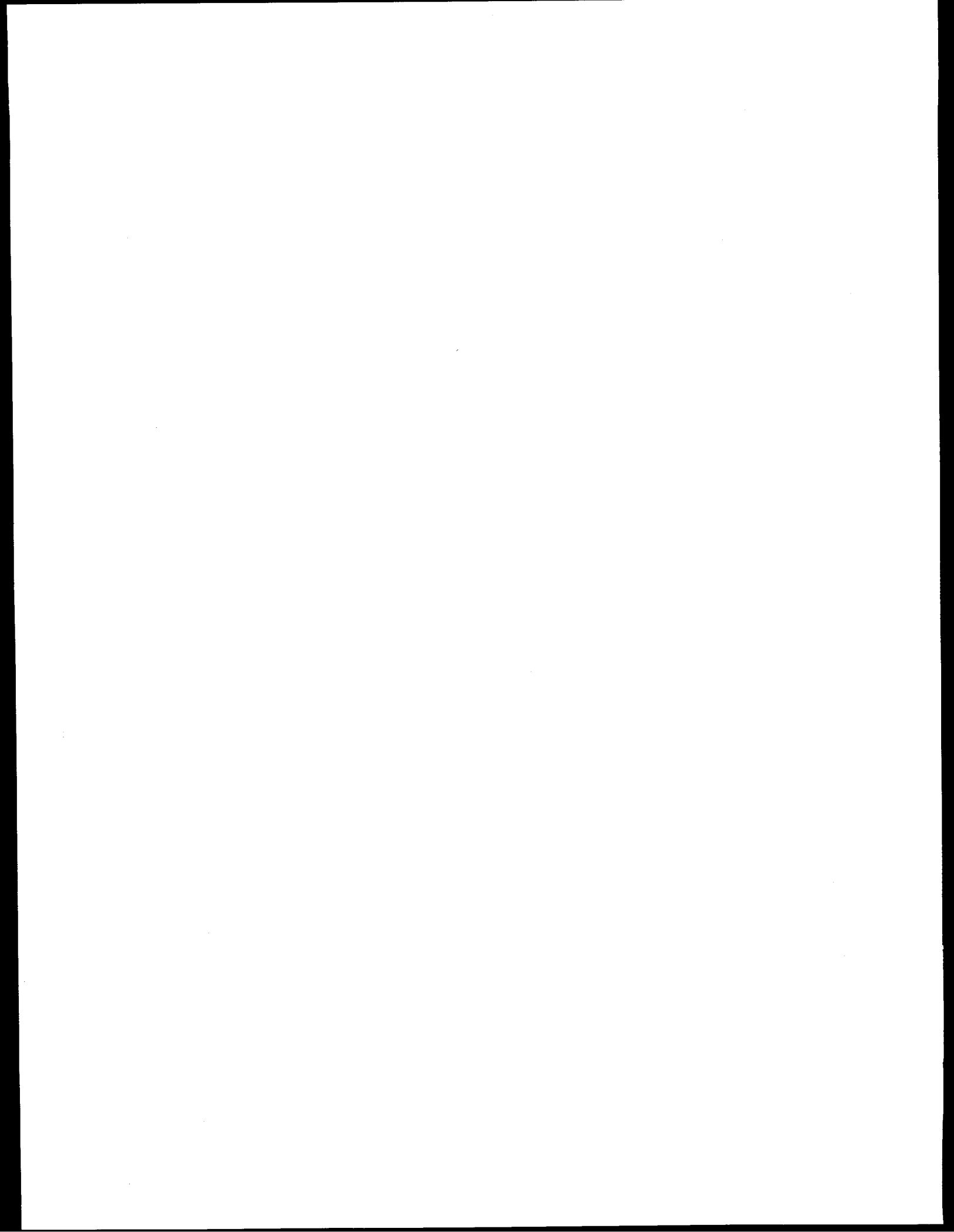
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Progress Toward Risk Informed Regulation

Commissioner Kenneth C. Rogers
U. S. Nuclear Regulatory Commission

1. INTRODUCTION

Good morning. It is truly a pleasure to be with you this morning to participate in the Twenty-Fourth Water Reactor Safety Information Meeting. One of the advantages to being the first speaker is that I can pose a variety of philosophical or practical questions, and pretend that subsequent speakers are somehow obligated to provide any answers that I lack. I intend to make full use of that privilege.

A significant part of this meeting, including the panel discussion immediately following my remarks, will be devoted to research related in some way to risk informed regulation. Before you get into the detailed technical discussions of specific research projects, I thought it would be useful to reflect for a few minutes on where we stand with respect to risk informed regulation, and why the research you will be discussing during the next few days is so important.

I plan this morning to do two things with this opportunity to speak to you. The first is to discuss the status of risk informed regulation from my perspective as a Commissioner, and offer some comments on its history and probable future. The second is to highlight some important milestones we have reached, a number of assumptions we have made, and some implementation issues that must be resolved or at least acknowledged.

2. PROGRESS TOWARD RISK BASED REGULATION

For the last several years, the NRC, with encouragement from the industry, has been moving in the direction of risk informed regulation. This is consistent with our regulatory principle of efficiency, formally adopted by the Nuclear Regulatory Commission in 1991, which requires that regulatory activities be consistent with the degree of risk reduction they achieve. Probabilistic risk analysis has become the tool of choice for selecting the best of several alternatives.

Closely related to risk informed regulation is the development of performance based rules. Such rules focus on the end result to be achieved. They do not specify the process, but instead establish the goals to be reached and how the achievement of those goals is to be judged. The inspection and enforcement activity is based on whether or not the goals have been met.

The most significant milestone in the development of risk informed regulation in the United States was the development of quantitative safety goals and their endorsement in a Commission policy statement in 1986. This addressed the question of "how safe is safe enough?" In the years following the safety goal policy statement, a number of internal NRC studies looked at the relationship between specific regulatory requirements and risk reduction or lack of it. In 1994, the Commission approved a probabilistic risk assessment implementation plan. The plan addresses the use of PRA in all major NRC functions; reactor regulation, research, evaluation of operational data, utilization of nuclear materials, and waste disposal. Its major elements include developing decision criteria for regulatory applications of PRA, developing pilot projects to test PRA application in specific circumstances, looking at the contribution of risk based thinking to the inspection process, and examining operator licensing issues from a risk perspective.

The PRA implementation plan recognizes that PRA will not become an effective regulatory decision making tool as long as its perspective and methodology are confined to a relatively small number of highly trained specialists. Its benefits can only be realized if a large fraction of the regulatory staff is not only familiar with its benefits, but is also actively involved in identifying and developing applications.

Following the PRA Implementation Plan was the publication of the Commission's PRA Policy Statement in 1995. The PRA policy statement formalizes the Commission's commitment to risk informed regulation. It states, in part, "The use of PRA technology should be increased in all regulatory matters to the extent supported by the state of the art in PRA methods and data, and in a manner that complements the NRC's deterministic approach and supports the NRC's traditional defense in depth philosophy."

An important element of the PRA implementation plan is the framework which the staff has developed for applying probabilistic risk analysis in reactor regulation. The framework has four parts; identification of regulatory activities, deterministic considerations, probabilistic considerations, and integration of the probabilistic and deterministic considerations.

The first part, identification of regulatory activities, defines the regulatory areas where PRA can play a role in the decision making process. The second part, deterministic considerations, assures that the current deterministic engineering approach is maintained unless a solid basis for change is established.

The third part of the framework is probabilistic considerations. Key elements include use of established methods, success criteria, human and equipment reliability data, and sensitivity and uncertainty analyses. The final part is the integration of deterministic and probabilistic considerations. Here is where a consistent and understandable combination of traditional deterministic information and new probabilistic insights must be constructed. This process should result in a reassessment of the bases of existing regulations, with those bases maintained unless a change is supported by the new, probabilistic information. The integration process would be carried out so that the net changes in risk are small, and that overall risk is consistent with high level guidance such as the Commission's safety goals.

As part of the PRA applications development process, there are currently a half dozen pilot projects being conducted by the staff and industry. These projects are intended to provide insights into the identification and treatment of deterministic issues, selection of risk metrics, and the development of PRA review guidelines.

3. VIABILITY OF RISK INFORMED REGULATION

It is evident from these NRC activities that we are firmly committed to pursuing risk informed regulation. Before looking at progress and problems in more detail, I want to comment on the apparent consensus within the nuclear industry that risk informed regulation is a good thing to pursue. In reality, this consensus is built on two fundamental assumptions.

The first is that risk reduction and risk management are high priority regulatory goals. I think this is a correct assumption, but it should be examined occasionally to remind ourselves that it is not immune from challenges.

The second assumption is that risk can be estimated by a formal, agreed upon methodology such as we currently are using in the nuclear industry. It is here that the importance of these basic assumptions starts to become evident. Even if it is easy to agree that risk reduction is a valid regulatory goal, it is easy to understand skepticism about the validity of the results derived from highly complex analytical models supported by uncertain parameters.

In the United States during the last several years, there has been a growing awareness of probabilistic risk assessment and its possible application in assisting a variety of technical and public policy decisions. This awareness has generated a spectrum of reactions, ranging from enthusiasm to hostility, depending on one's view of technology and politics. The U.S. Congress, for example, has been unable to agree on legislation that would mandate risk assessment and cost/benefit analysis as the preferred tools for decision making in a variety of regulatory activities. One cannot rule out the possibility that future political debate on the acceptability of risk assessment will impact its use in nuclear safety regulation.

The fundamental point here is that regulation is a political process, not a scientific one. It can be successful only if the process and its results are accepted by the public and by their elected representatives. Public trust requires that the regulatory process be open and understandable. Probabilistic risk assessment is neither simple nor easily understood, and as we increase its use, we will have the corresponding challenge of public acceptance.

4. WHY ARE WE CONSIDERING RISK INFORMED REGULATION?

The authorizing legislation for the Nuclear Regulatory Commission requires that the Commission ensure that licensed activities be conducted so as not to result in "undue risk" to public health and safety. As a result, the Commission has considered that there is a level of protection to the public, referred to as "adequate protection," that must be maintained in licensed activities regardless of cost. Beyond this level of "adequate protection," the NRC can establish requirements that result in "substantial improvements" in safety, as long as the benefits of such requirements outweigh the costs. The Commission believes that our current regulatory process has resulted in adequate protection of public health and safety. Therefore, when we commit to moving toward a new regulatory regime, there is an implicit assumption that the reason for doing so is to accomplish something other than adequate protection.

It is probably true that reasons for considering change depend on one's viewpoint. For example, I strongly suspect that enhanced protection of public health and safety is uppermost in the minds of the regulatory staff. Licensees, on the other hand, see opportunities to reduce regulatory burden without reducing safety. While not discounting the value of either viewpoint, I suggest there are other equally important reasons for moving toward risk informed regulation.

The first reason is to rationalize our system of regulation. The regulatory structure and process in the United States evolved in parallel with the technical development of the nuclear power industry. The initial rules and regulations had to provide adequate protection for public health and safety, while still allowing enough flexibility for the successful development of the technology. Rules were written as they were needed to resolve specific problems. Design conservatism was used to compensate for the lack of performance and operating reliability data. The fundamental design and regulatory philosophy was "defense in depth." The accident at Three Mile Island focussed industry and regulatory attention on the crucial importance of operations, but the emphasis was still on identifying rules or requirements judged to be important to safety, and then assuring compliance with those rules and requirements.

We believe that this system, as it evolved, has indeed resulted in adequate protection. In addition, we suspect that for the nuclear power plants operating in the United States, we are meeting or exceeding the higher level of safety articulated by the Commission's safety goals. A fully defensible conclusion that U.S. nuclear power plants are meeting the safety goals probably cannot be reached without having a Level 3 PRA for all operating plants. Such analyses are within our technical reach. It may also be within our technical reach, therefore, to determine if the present collection of regulatory requirements is sufficient to achieve the safety goals.

Current design and regulatory practices include the NRC's general design criteria, Regulatory Guides, and an established spectrum of design basis accidents with acceptance criteria. Can we establish that the current combination of design rules, regulations, and practices leads to a predictable level of safety? I believe that one important result of the move toward risk informed regulation should be to establish the relationship between a system of design related regulatory requirements and the resulting level of safety. I should acknowledge that this idea was raised by Dr. Thomas Kress, the Chairman of the Advisory Committee on Reactor Safeguards, a couple of years ago, and I continue to believe that it is worth pursuing.

There is a parallel question that can be raised about the relationship between the regulation of facility operation and safety. Thus, a second reason for moving toward risk informed regulation is to help us establish the relationship between safety and specific regulatory activities related to facility operation.

The NRC staff considers the regulation of operating facilities in terms of three major functions: licensing, inspection, and performance assessment. Licensing deals with the interface between the design, operation and specific regulatory requirements such as technical specifications. Inspection involves the direct observation of licensee hardware, procedures, and operation of the facility. Performance assessment verifies that plants are operated safely, and searches for trends that might indicate a future decline in safe operations.

It seems to me that specific regulatory activities must satisfy at least one of three general purposes. There is agreement among regulators and licensees that the responsibility for safe operation of a facility lies with the owner of that facility. The first regulatory purpose, then, is to ensure that the licensee accepts that responsibility, and that regulatory action does not diminish it. The next logical step in the process, and therefore the second regulatory purpose, is for the regulator to assure itself that the licensee is conducting its operations so the desired level of safety is achieved and maintained. Viewed in this way, it becomes clear that the regulator's function is not to step in and provide safety if the licensee falls short. Rather, the regulator's activities should be designed to evaluate the relationship between the licensee's activities and safety, and to warn the licensee when that evaluation indicates a decline in safety.

The third purpose of a regulatory activity is to provide assurance to the public that the regulatory agency is doing its job. A high visibility failure at a nuclear site with little safety significance may cause unacceptable damage to the credibility of the regulator and the industry. Adequate resources must be allocated to issues that are of high public concern, even if technically we rate their safety significance as relatively low.

As we move toward risk informed regulation, we must establish an understanding of the relationship between specific regulatory activities, facility safety, and public trust.

5. HOW SHALL WE PROCEED?

In the first part of this talk I briefly described the framework that the NRC staff has developed for the implementation of risk informed regulation for operating power reactors. I think that framework is well thought out, and it reflects the deliberate, conservative evaluation process that should be used when changing a system that is already accomplishing its intended regulatory purpose. There are, however, a number of facets to the move toward risk informed regulation that are worthy of comment.

Traditional regulation, as it exists today, is a combination of deterministic, prescriptive, and risk based concepts. In spite of its lack of coherence, traditional regulation, properly executed, has accomplished its purpose. There is much to be said for conservative design, conservative operating philosophy, and defense in depth. They have served us well, and should continue to be part of our safety structure.

In spite of the preceding discussion, it will not be sufficient to simply consider new activities in a risk informed way. A risk informed perspective will allow us, as a regulator, to limit new regulatory requirements to those that bring about a significant reduction in risk. We also have an obligation to examine existing requirements to see if they are causing the expenditure of resources out of proportion to their safety benefit. If we require the expenditure of resources on activities of little safety benefit, we guarantee that those resources will not be used to enhance safety. We should ensure that sufficient attention is devoted to activities that can be changed in a beneficial way or eliminated.

The cost of change itself needs to be kept in sight. Changes that may be worthwhile for a facility that has 30 years to operate, may not be beneficial for a facility that will only operate for another five years. Sometimes a simple, somewhat arbitrary rule accomplishes a desired result more effectively than a more precise, risk informed, but complicated probabilistic guideline. In some cases, it may turn out that it is not the rule or regulation, but the way it is implemented or enforced that should be changed. Common sense should continue to prevail.

Earlier this year, the Advisory Committee for Reactor Safeguards wrote to the Commission endorsing the continued move toward risk informed regulation, and identifying what it believed to be the next logical steps in the process. Among the most important issues identified in that letter is a need to restate the Commission's safety goals policy in a way that allows the goals to be used on a plant specific basis. The Committee also cited the need for a methodology to determine performance-based criteria for regulatory action that are fully consistent with the top level safety goals.

There is one last point that I think is worthy of attention. At least in the United States we have been closely linking the terms risk informed and performance based. I mentioned performance based rules in my introduction, and the concept can be articulated in a rather concise way. I believe that the practical relationship between risk informed and performance based will be somewhat more complicated. They are two entirely separate concepts. It may be possible, desirable or even necessary to link them in specific applications, but they are not invariably bound together.

Six years ago, when the Commission was seeking outside views on non-prescriptive regulation, we heard from Marshall Breger, Chairman of the Administrative Conference of the United States. The Administrative Conference had seen the issue of design standards versus performance standards debated in a variety of regulatory situations, and Breger stated that performance standards come with their own set of problems. The usual result of a performance based rule is that it becomes more difficult to verify that the intent of the rule is being met. As an example, he cited a change in a fire safety rule by the Occupational Safety and Health Administration. When a specified height for mounting fire extinguishers was changed to a performance based rule stating that fire extinguishers must be "accessible," the burden of compliance for industry became more difficult. We should expect similar difficulties.

6. CONCLUSION -- WHAT NEEDS EXTRA EFFORT?

What should we conclude? My own view is that risk analysis can contribute to our understanding of nuclear safety, and can help inform decisions. I also believe that, in the limit, regulation should be a mixture of deterministic and probabilistic considerations. We are, and should be, moving in the direction of risk informed regulation, but our final destination includes a strong residual of deterministic requirements.

The success of risk informed regulation ultimately depends on having sufficient reliability data to allow quantification of regulatory alternatives in terms of relative risk. Similarly, the success of performance based rules depends on having sufficient performance data to provide assurance that goals have been achieved. The NRC is considering a new rule which would require power reactor licensees to collect and report to the NRC certain equipment reliability data. Although the industry appears to agree that reliability data are

needed to move forward with risk informed regulation, it is opposed to a rule requiring data collection. It would prefer to rely on a voluntary program to produce the required data. Regardless of how this is finally resolved, there is a clear need to collect, evaluate and disseminate relevant data throughout the nuclear community.

Perhaps the area of human reliability and organizational performance has the greatest potential for improvement in reactor safety. Our ability to model human performance is significantly less developed than our ability to model mechanical or electrical systems. Indeed, some experts argue that today's hardware is good enough, and that the greatest safety gains can be realized by improving our ability to predict, and modify, human performance. More work needs to be done on how to model human performance in probabilistic analyses, and how to collect data on human performance so that the human performance contribution to core damage frequency can be quantified. At the organizational level, there is a need to characterize management practices that contribute to safe operation so that those practices can be established and maintained. Most importantly, is there any way to identify early warning signs of changes in an organization that foretell a decline in operational safety? Developing reliable insights on human reliability and organizational performance is a formidable task, but one that should not be avoided.

The move toward risk informed, performance based regulation provides an unusual, perhaps unique, opportunity to establish a more rational, more effective basis for regulation. Performance based rules offer the opportunity for facility operators to claim a greater ownership of the responsibility for facility safety. Quantitative measures of risk that are understood by both the operator and the regulator will increase the regulator's confidence that facility safety is maintained.

Unfortunately, the third regulatory objective, assuring the public that facilities are operated safely, may not be helped by risk informed, performance based regulation. Prescriptive, deterministic standards and compliance with those standards may be more easily accepted. Ultimately, compliance measurement could control the form and content of our rules. As you all know, the NRC and a couple of its licensees in the Northeast have attracted a great deal of negative publicity this year. One of the central issues was a discrepancy between a licensee's refueling procedures and the representation of those procedures in the plant safety analysis report. Nearby residents were understandably upset, and had harsh words for both the NRC and the utility. I do not recall that a single member of the public demanded to know how much the core damage frequency had been increased by the questionable refueling practice. They did want to know why the utility was not in compliance with its safety analysis report. They understood compliance.

As I promised, I have raised a number of questions, I have suggested answers to a smaller number, and I will expect the rest of the speakers to provide the missing insights. Thank you for the opportunity to speak to you. I hope the next few days will be both interesting and productive for all of you.

Research Needs for Risk-Informed, Performance-Based Regulation

Jack A. Bailey
Palo Verde Nuclear Generating Station
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Palo Verde has used PRA-derived risk insights for about 10 years now. We originally started applying PRA modeling to an auxiliary feedwater system during our initial licensing phases of the plant, and as a result of that, we were able to work with the NRC and apply some graded quality requirements to that particular system.

There was a third redundant auxiliary feedwater pump, and we now can treat that system as partially safety related and partially non-safety related. So it was an advance for us at that time to be able to make decisions with a PRA and we began learning how to use those techniques.

After completing the IPE it became natural for us to make a transition into other areas at the plant to look for areas where we could apply the insights gained from PRA into our decision-making processes. Those that we embarked upon initially were areas where we could gain operational risk assessment insights.

As an example, we have a sensitive-issues manual at the plant that we developed that's available in the control room for the control room operators; and this manual defines certain activities in the plant that, based on conclusions we derived from our PRA studies and our IPE and the other work we had done, were higher risk activities for the plant.

What it does is establish certain administrative controls and criteria that the operators need to go through in order to ensure that those activities are done more safely. A good example would be additional pre-job briefs or maybe an additional engineering review of certain activities before they're accomplished. So that's what the sensitive-issues manual has done for us.

We also looked at scheduling of normal activities at the plant from a maintenance perspective, and we had developed about -- shortly after the IPE submittals had taken place -- a 12-week rolling schedule where certain activities were going to be pre-planned on an ongoing basis. We're able to, as part of that, use PRA insights to look at where the peaks were in the plant risk taking place and where we should apply those to try to minimize outage times or move certain activities from one place to another. Again, this was a natural outgrowth of the information and knowledge gained from our PRA work.

However, once we had established that baseline risk and how to manage it from a plant point of view, the industry, as well as Palo Verde, started asking the question: "Well, what happens for emergent work or off-normal work that takes place?" So again, from an operational perspective, we used our PRA insights to develop what we called a maintenance matrix available to the control room operator that allows them to make decisions in the control room about what type of increase in risk they're experiencing as a result of certain equipment being taken out of service.

For example, you have your 12-week rolling schedule that says here's the risk if all things go according to schedule. If a new component or system is taken out of service as a result of an emergent problem, it helps in quickly ascertaining whether you are now in a high risk category, a low risk category, or a neutral risk category.

We, along with the rest of the industry, have moved into an area of trying to evaluate shutdown risk. We do not have a full model PRA for our shutdown scenario as yet, but we do use our PRA techniques in our review processes to look at all outage activities and try to schedule certain activities so as to minimize plant risk during the outages.

And finally, probably the best application we've seen as an industry, is the maintenance rule where we have combined some performance-based criteria with the insights gained from PRA, and we've been able to establish a sound maintenance-rule program as a result of that.

Following that application of the PRA insights, we looked at other applications that could be a benefit to the facility. In particular, we knew that a number of our technical-specification-allowed outage times were initially based on best engineering judgment, and we had a lot of data now available and information from risk techniques to say, "Can we gain more benefit from the plant's perspective by extending some of those allowed outage times without any impact on safety or overall plant risk?"

It turned out there were a number of areas where we could do that. We're working with the Combustion Engineering owner's group and other utilities to try to address some of these, for example, diesel generator maintenance, testing, safety injection tank, and our low pressure safety injection system -- all looking to be candidates for some of those advantages.

We at Palo Verde had an advantage when we started looking at procurement and graded quality in that we had a QA plan at Palo Verde that already allowed for the application of graded quality principles. Therefore, if we could come up with the basis for reducing the amount of quality oversight applied to certain of those activities, it was already acceptable within our plan. So we became a natural candidate, I think, for movement into a pilot program for graded QA without having to have additional regulatory approval on changing our QA requirements.

We started looking at our materials process and our procurement, and we started applying the risk insights gained from that to a component level and to a sub-component level on the procurement of parts. We moved beyond that recently in the last year or so and working with another utility, Texas Utilities, on becoming a pilot program for the application of risk insights to inservice testing.

This is a natural outgrowth of the Tech. Spec. testing and allowed outage times in that we now have a technique to look at how frequently we test certain components in the plant and whether or not that frequency is adequate or necessary for safety. In some cases, we had to add testing where it was not previously required. So it works both ways for the utility; it's not all a matter of trying to reduce the amount of testing that is done at a plant. It's really application of the right amount of testing to the right components at the plant. And risk insights allow you to make those decisions.

Finally, even though we're not a pilot plant in this area, we're certainly closely tied to the industry efforts in trying to assist through the owner's group the efforts to look at inservice inspection activities at nuclear power plants and how we can apply risk insights to those areas.

Palo Verde believes, right now, that risk-informed decision-making does enhance safety. It has given us some insights and some information that we previously would not have seen through deterministic efforts. We also, though, believe that it should be implemented in a manner that's appropriate to each utility, and, in some cases, the rules that apply to one utility don't necessarily fit entirely to another utility. So you need to understand the process and how it fits in the overall efforts.

But it also leads to the ability to critically apply limited resources and do it in the right areas, and that's very important as we move toward competition in the industry to make sure that we maintain our focus on the right things that enhance and maintain plant safety.

The other thing I will tell you about these areas is that we have made maximum use of the expert panel in all of these applications. So with the blend between risk insights and the use of the expert panel, we would conclude right now that it's absolutely fundamental to make these processes work well -- at least over the near term as we keep trying to look for ways to make them work better.

What I want to focus on now is four areas where we believe additional research based on our experience and lessons learned could be valuable and useful to the industry. These are not new items and new areas, they have been talked about amongst the PRA experts and amongst the industry experts for a while, but it's to give you a feeling concerning some of the limitations, some of things that we need to look at as we move forward.

Commissioner Rogers did indicate that we have established safety goals for the application of risk insights into the industry. But they also tend to be a little bit generic in that they don't really lead to a hard conclusion about what the level of safety is at a particular facility.

As an example, a facility that sits in the middle of a desert with a low population surrounding it would not have the same consequences on certain accidents, scenarios, or certain other decisions at the plant as one that's in a heavily populated area if, in fact, you're worried about the health to the local community public that's surrounding the plant.

So even though we do have safety goals, I believe there's still some work to be done as we try to get more quantitative with the results of PRA as an industry to understand what the real safety impact is of those goals. We believe that the current safety goals are adequate; but as we continue to focus more and more on what this tool can do for us as an industry; that's an area that probably deserves additional review and thinking.

However, the biggest gain so far in the industry does not come from the absolute number that comes out of the overall plant risk assessments, but it comes from the relative ranking of different activities to each other or different components to each in the process. So relative ranking has still proven valuable and will continue to prove valuable in those efforts; and we get a lot of bang for the buck just based on doing that activity.

Another thing that falls out of this, though, is: should we allow, once we establish a baseline mean core damage frequency or other safety goal or safety result, changes that increase that over time? Palo Verde's position based on what we've learned is that, yes, small increases in safety are appropriate and should be allowed, but the amount to which they should be allowed has not been defined nor has a standard been established for the industry.

So at this point while it appears appropriate, the degree at which it is appropriate is still probably a ripe area for additional research and information. There are industry guidelines right now that are making proposals in that area, and they seem like they're well founded, but they haven't been bought into entirely; therefore, we need to continue to move until we understand what we're allowed and what we're not allowed in that area.

The other area which is equally or probably maybe even more difficult is where do you allow trade-offs from a risk standpoint in your overall plant assessments? If we decide to increase risk slightly in our inservice testing program requirements, for example, by making decisions, can we offset those risks by something over

our allowed outage times on Tech. Specs. since I've used those two examples already? Where can we make appropriate trade-offs and where is it not appropriate to do that? So that's an area that deserves additional attention, too.

Once we understand what the acceptable risk is, and what it means from a safety perspective, we move into an area of what type of acceptance criteria should be used by the industry for making decisions. Things that have been used so far are importance measures.

Our concern, I think, from Palo Verde's perspective is that there are no standards that make those final as far as our decision-making techniques; they could change over time, and when they change, it does result in some changes in what the risk information is telling you. So what we would like to see is certainly a consensus amongst the industry and amongst the regulators on what the importance measures are and as we go into the future using these techniques.

The values that you use in ranking are also very critical. Let me give you one example that I think you'll understand. When we went through the inservice testing program, we went through the EPRI guidelines, PSA guidelines, on how to rank certain valves in terms of frequency of testing. Based strictly on the thresholds that would be used for those importance measures, there were a number of valves for which you could stretch out the frequency; we didn't need to test them as frequently.

When we plugged back into our PRAs, some of the results -- because if you increase the testing frequency of valves, the probability of failure will increase -- the actual PRA results increased 300 percent. That was an unacceptable response.

So what we found is that if you have too much adherence to the thresholds that you use to categorize things as low, medium, or high, you actually can have inappropriate conclusions on the overall risk to the plant based on the net sum of the risk analysis.

So the point is that those thresholds are important. You cannot separate the threshold from overall plant risk. But right now there is no direct tie on those thresholds and what the actual impact is on plant risk as they are used.

We saw the same thing when we went through the maintenance rule -- where do you make your cutoffs on high versus medium versus low risk from a maintenance rule perspective? Clearly there is certainly some wobble room in those decisions and those decisions are not directly tied to the overall risk results. So you do have to go back through and do the PRA based on the results to see where you actually stack up.

The last item on the slide deals with measurable parameters. We don't currently have good measurable parameters on how to evaluate what we're trying to achieve in certain areas.

We know that we're trying to maintain safety, but we also know that in the area of graded procurement, you're going to stop doing some things you've done from a quality point of view on a number of parts. It looks like from the assessment that this is going to be okay, but without having good measurements to indicate when it is okay and when it is not, you are increasing risk.

The maintenance rule is just the opposite. In the maintenance rule we typically are focusing resources to do more in certain areas than we might have done previously. So if anything, the conclusion up front would be that things are probably going to improve, not get worse. So you have those different types of programs to which you have to apply these techniques. Procurement, since you're doing less, you would have to conclude there is an increase in risk -- how much that increase is may not be fully understood. But in another area,

you know you're going to decrease risk because whatever you're doing is going to have a positive effect on what you're trying to accomplish.

The third area is the technical quality of PRA -- I won't spend a lot of time on this. Clearly, there is a wide diversity of techniques used. Ashok Thadani indicated that, too, from their perspective. There are no standards right now that established how the NRC should be using certain methodologies or techniques. And the industry experts themselves, when they get together and talk, find that they are not doing it entirely the same and making decisions the same.

I believe that standards would be useful in this area. We don't need to be overall prescriptive with them, but I think we do need to tighten up where we're going with that. There is an initiative right now to do assessments on a peer basis within the utilities where they take their experts and go look at each other's PRAs, and also to develop a certification process of PRAs. I think all of this is useful and valuable. It will improve the overall quality of PRAs.

However, without standards being established, the tendency will be for everybody to add best practices from each other's utilities into what each other is doing. And if you're always adding best practices, eventually you end up with overkill type program on some of the things you have in it. While it will be good and it will be moving in the right direction, it does not tell you what is the minimum amount or what is the standard we have to achieve in order to know whether we've done enough in those areas.

So there is a balance there where we need to do some work on standards and also need to continue forward with the efforts to help each other and make sure our PRAs are adequate.

And finally, the fourth one which has already been mentioned briefly, is data issues. Right now, Palo Verde's PRA and the results from it are largely built upon generic industry data, and that's true, I think, for most of the industry. There is plant-specific information in it, but it's limited. The other issue that's a concern is that most of the generic industry data is dated.

For example, the efforts over the last 2 years to move toward better maintenance at many utilities has done a lot to improve reliability of equipment in those plants. That latest information is not contained in those data bases that we used for the PRA. So from a perspective of what are the results telling us, it's probably overly conservative at this point because it's using old failure data.

But on the other hand, there could be new failure data that also is worse for some components -- there's a time lag, a large time lag in the way data is used and obtained for the purposes of using PRA. So there's a lot that can be done there, and we don't need to dwell too much on it.

The last one, though, the industry data bases that they have been using, NPRDS and failure data trending, while useful, are limited. For example, even though they'll have failures in it, they don't tell you what the number demands or how frequently the component was used in terms of looking at that data. So those types of things, from a risk insight point of view or how you're going to use them in risk insight programs, do indicate a lot of work needs to be done.

One way to do that is to do a pilot of a number of utilities. Instead of having every utility have to input all their data to make this happen, I think you could go around and select a few utilities that have equipment that's representative of certain areas and do a quick-and-dirty study that would allow us to get additional data in the short term that would be useful for the purposes of forming risk insights.

So in conclusion I'll make a couple of statements. One, we believe from Palo Verde's perspective that the tool itself has allowed us to focus our resources on things that have high safety impact on the plant; and as a result, we have actually improved plant safety over the last number of years.

We have expanded the knowledge gained from risk insights into operations, into management, into other areas, but there's a lot of work that still can be done there; a lot of work that can be done without changing anything in terms of how we're doing business today with the PRA, and that's a focus we need to *continue* to work on.

This tool is also useful and important for us in today's climate because it does allow us to focus plant resources on the right things and therefore to reduce cost in areas where the resources don't have to be used. In low safety impact areas, those resources can be moved and focused on the high safety impact.

The key, though, is that the industry needs to learn how to use this tool better. We've come a long way. Plants like South Texas, Texas Utilities, Palo Verde are out there piloting a number of these. We have learned a lot, but there's an awful lot that we still can do. More research does need to be done in this areas.

We need to be deliberate in that approach, and so I like probably the blend of moving forward but moving forward with caution. We cannot afford to try to move down this path and make errors in it or make judgment mistakes in it, and therefore, set the entire industry in this effort back because it is a very valuable and useful process.

The other thing to keep in mind, the final comment I'll make, is that even though this can be extremely complex, it doesn't need to be that much more complex than it already is. We need to do everything we can to make these changes and apply the research to keep it as simple as possible. Because ultimately we need to be able to apply it with consistency and predictability at licensed facilities and get the entire staff to understand this process. And the only way to make that happen successfully is to do it with simplicity.

Palo Verde PRA

- ◆ **Palo Verde uses its PRA Organization in Applications Beyond the IPE**
 - **Operational Risk Assessment**
 - **Shutdown Risk Assessment**
 - **Maintenance Scheduling**
 - **Maintenance Rule**

TRACKER ID

Industry Involvement

- ◆ **Heavily Involved in Pilot Activities**
 - **Technical Specification Allowed Outage Times**
 - **Graded Procurement/QA**
 - **ASME Inservice Testing**
 - **ASME Inservice Inspection**

Acceptable Risk

- ◆ **Plant Specific Application of the Safety Goals**
- ◆ **Use of Quantitative Health Objectives**
- ◆ **Relative Ranking**
- ◆ **Risk Neutral vs. Risk Increase**
- ◆ **Balancing Risk / Risk Credit**

TRACKER ID

Acceptance Criteria

- ◆ **Standards for Selection of Importance Measures**
- ◆ **Values for Ranking Based on Importance Measures**
- ◆ **Measurable Parameters to Monitor Plant and Licensee Performance**

Technical Quality of the PRA

- ◆ **PRA Assessments**
- ◆ **Industry Certification Process**
- ◆ **Dealing with Uncertainties**

Data Issues

- ◆ **Generic Industry Data Pre-dates Recent Industry Improvement Efforts**
- ◆ **Maintenance Rule Industry-wide Data Warehouse**
- ◆ **Failure Data Trending vs. Complete Data Trending**

Desired Future State

- ◆ **The Industry has a Tool to
FOCUS RESOURCES on High
Safety Impact Issues**
- ◆ **The Industry has a Tool to
REDUCE COST on Low Safety
Impact Issues**
- ◆ **The Industry Needs to use This
TOOL**

5. The fifth part of the document outlines the various methods used to collect and analyze financial data, including the use of spreadsheets, databases, and specialized accounting software. It also discusses the importance of regular audits and the role of external auditors in verifying the accuracy of the financial statements.

6. The sixth part of the document focuses on the importance of budgeting and financial planning. It discusses the process of setting financial goals and the role of the accounting department in monitoring progress and making adjustments as needed. It also emphasizes the importance of communicating financial information to management and other stakeholders.

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Research Needs for Risk-Informed, Performance-Based Regulation

T.H. Cloninger
South Texas Project
Houston Lighting & Power Company

I am an executive at a utility that operates a large nuclear facility. We do look at the PSA and risk-based operation as well as risk-based regulation as a tool that will help us to better apply our resources to maintain the required level of safety to protect the public health and safety.

And I'd like to make it very clear that we do not look at risk-based operation decision-making or risk-based on-line maintenance, etc., as a tool to reduce or marginally reduce safety. We believe strongly that required public health and safety is not only a moral obligation, but it's a business obligation. And I think our friends in the northeast will be the first to tell you that anything that's perceived as a reduction in safety is certainly not a good business decision.

If I could have the first slide. Obviously, the first most publicized and well-known application of risk-based regulation was the maintenance rule. We at South Texas were one of the pilot plants for the first inspections of the maintenance rule. We believe that it has shown that there are opportunities and challenges in the risk-based regulation arena. And I'd like to emphasize that we believe there are opportunities as well as challenges. Next slide, please.

We think that win-win opportunities exist in areas of improved overall plant safety hand in hand with improving cost efficiency. And perhaps most importantly, we believe that it can form the basis to stimulate innovation and continuous improvement that would lead to our long-term competitiveness.

I'd like to pause to make one comment that's probably our greatest application right now -- PSA or risk-informed decision-making in our on-line maintenance activities -- and that has led significantly to our ability to perform maintenance safely and reliably while the plant is at power. It has also led directly to our strong reduction in refueling outage duration. Next slide, please.

As I mentioned, opportunities go hand in hand with challenges and we do believe that challenges exist in certain technical areas such as identifying appropriate performance parameters that support continuous improvement to build data collection processes. You heard Jack and Ashok mention a number of these. I'd like to emphasize the last bullet up there -- I believe that it's tremendously important as we go forward in this area that we maximize the use of existing programs and processes rather than reinventing the wheel, which, to a large degree, we saw in the maintenance rule application. Next slide, please.

So how do we meet these challenges while working hand in hand and building off the opportunities to improve? I see that we need research in two time areas -- what I would call short term needs in the next 1 to 3 years, and the longer term needs beyond 3 years. I'd like to comment on each of these areas a little bit. I won't hit each bullet, but I think that there are some areas that I would like to emphasize.

We do believe that research is needed to resolve differences in initiating event frequencies for similar events at similarly designed stations. These differences should not, in general, exist; however, they do, as we all know. We also believe in research to determine at what point something ceases to be significant. I think that this is a point where we see PSA supporting the appropriate demarcation from deterministic licensing

basis; wherein, an arbitrary designed basis assumption may, in fact, be, from a PSA standpoint, relatively insignificant.

I'd like to comment, also, that we need -- we think that we need -- to define in quantifiable terms acceptable methods for risk significant determination and establishment of site-specific risk thresholds rather than the arbitrary risk thresholds that many of us use now for such things as on-line maintenance decision-making.

In the areas of long-term research needs, I believe that we do need -- and I think that this is an area that's been touched on also -- research to determine the impact of organizational effects on operator and equipment reliability. Research to determine failure rates for inherently reliable components such as passive components like tanks, reservoirs, etc. And also, further research on our IPEs. Next slide, please.

In the area of performance data, I'd like to touch on a few things in a little more detail. I won't comment on all of these areas. But I would like to say that I do believe that more is needed in the area of acceptable processes for expected events. These are things that perhaps -- given the life of the plant -- we do know that we have the potential of seeing an event of this type, and we need to have a more in-depth understanding of its effects on overall plant response. And also, as Mr. Thadani talked about, the area of common-cause effects on some of our equipment. Next slide, please.

In the area of PSA data, we feel that further research is needed on the impact of quality assurance and other soft issues on the control of plant equipment reliability and, as mentioned before, the areas of aging and environmental degradation effects on overall reliability and, as I also mentioned before, failure rates for passive or inherently reliable components. Next slide, please.

Perhaps, the most exciting, yet probably the most controversial, is the area of organization and human performance. I think that, if we are going to move forward with significant improvement and understanding of plant performance, we need to understand the impact of human performance in the day-to-day operations at the facility. Some of the areas that I've highlighted there for research, I think, will give us a greater insight into this most important area.

We at South Texas believe that the use of probabilistic safety assessment, risk-informed regulation, as well as risk-informed decision-making is a very important tool. But I would emphasize that it is a tool. We believe, as Jack mentioned earlier, that there needs to be a fundamental understanding of that tool throughout the organization -- not only its strengths, but its limitations.

Research Needs for Risk-Informed, Performance-Based Regulation

Risk-Informed, Performance-Based Regulation
Experience to Date

- Maintenance Rule, 10CFR50.65
- *Opportunities and challenges are expected as risk-informed, performance-based regulations emerge.*

Research Needs for Risk-Informed, Performance-Based Regulation

Opportunities exist in areas to:

- Improve safety
- Improve cost efficiencies
- Stimulate innovation, continuous improvement, and competitiveness.

Research Needs for Risk-Informed, Performance-Based Regulation

Challenges exist in areas to:

- Identify performance parameters
- Build data collection processes
- Determine risk-informed, performance-based triggers for adverse trends
- Maximize use of existing programs and processes

Research Needs for Risk-Informed, Performance-Based Regulation

So, how do we meet the **challenges** represented by RIPB regulation while also building off the **opportunities** to improve safety and costs? How can research help us?

1 - 3 Years



"Short Term Needs"

3 - 5 Years



"Long Term Needs"

Research Needs for Risk-Informed, Performance-Based Regulation

1 - 3 Years
→
"Short Term Needs"

- Research to resolve differences in initiating event frequencies for similar events at similarly designed stations.
- Research to determine at what point does something cease to be significant.
- Research to determine if new importance measures should be developed.
- Research to define in quantifiable terms acceptable methods for risk significance determinations and establishment of site specific risk thresholds.

Research Needs for Risk-Informed, Performance-Based Regulation

3 - 5 Years

"Long Term Needs"

- Research to determine the impact of organizational effects on operator and equipment reliability.
- Research to determine failure rates for inherently reliable components.
- Research on Individual Plant Examinations to address impacts on uncertainties resulting from differences in technical approaches.

Research Needs for Risk-Based, Performance-Based Regulation

Areas of Research for Performance Data:

- ▶ **Initiating Event Frequencies For Rare Events (Best Estimates):**
 - Large LOCAs
 - Seismic Events
 - ATWC
- ▶ **Acceptable Processes for Expected Events:**
 - SGTRs
 - Small LOCAs
 - Loss of Offsite Power
 - Severe Weather Events
- ▶ **Common Cause Effects:**
 - Similar Equipment in a System
 - Similar Equipment in Different Systems

Research Needs for Risk-Based, Performance-Based Regulation

Areas of Research for Performance Data (cont.):

- Research for PSA Data
 - ▶ Impact of quality assurance/control on equipment reliability/performance
 - ▶ Account for Aging and Environmental Degradation Effects
 - ▶ Failure rates for passive or inherently reliable components

Research Needs for Risk-Informed, Performance-Based Regulation

Organization & Human Performance:

- Organizational Effects:
 - Research to correlate organizational factors to operator performance
 - Research to correlate organizational factors to performance shaping factors relative to human performance
 - Research to determine effectiveness of Expert Panels relative to equipment and station performance
 - Impact of Organizational Effects on Uncertainties
- Development of Risk-Informed, Performance-Based "Indicators"

Research Needs for Risk-Informed, Performance-Based Regulations

Ashok C. Thadani
U. S. Nuclear Regulatory Commission

The value of research cannot be understated in terms of ensuring the availability of sound technical bases for timely rulemaking and related decisions in support of NRC regulatory/licensing/inspection activities. Research plays a significant role in the formulation of new safety regulations and modifying existing regulations. Three of the more recently implemented rules that illustrate the important role research plays in their development are the regulations regarding pressurized thermal shock, anticipated transients without scram, and station black-out. All of these rules were developed using risk-informed insights that were made available through work completed by the NRC's Office of Research. The scope of activities undertaken by the Office of Research has provided an appropriate focus on safety and covers all aspects of the reactor program, including thermal-hydraulic activities, risk assessments, materials issues, and severe accident activities.

The first comprehensive risk assessment was completed by the Office of Research 21 years ago; WASH 1400 was published in 1975. We have made progress since then, but not enough. Over the years, a great deal of effort has gone into better understanding physical phenomena such as core melt progression and containment response. The level of research effort that has gone into better understanding such phenomena has been tremendous. Through these efforts we have developed reasonable thermal-hydraulic codes that enable us to better understand core melt progression and the ability of the containment to deal with the challenges that come from severe accidents. Although we have made much progress, there are still a number of uncertainties in these calculations that deserve and are receiving further attention. Nonetheless, the research conducted thus far has given us a better understanding of the ability of the structures to deal with these types of accidents.

Another top research priority is improving our understanding of the aging process in nuclear power plants. We need to thoroughly understand how aging of components could impact their performance and, ultimately, impact the level of risk. In this regard, a great deal of research effort is ongoing with particular focus on reactor vessels and steam generator tubes. The issue of steam generator tubes is very important from both a safety and economic perspective. It is critical that we understand the behavior of the tubes under challenging environments such as core melt conditions because the failure of tubes could provide a pathway for release of activity to the environment, bypassing the containment. This research activity is currently ongoing.

The work that the Office of Research has done in the area of severe accidents has really been the key to resolving some of the more complex technical issues such as direct containment heating. In addition, the research that has been conducted in the area of advanced light water reactors, focusing on the Westinghouse AP-600 design, and the experimental work conducted in Japan have been very helpful in resolving a number of difficult questions regarding the complicated performance of passive systems.

This background on regulatory applications of research is intended to illustrate that the scope of regulatory research activities is extremely broad and closely linked with gaining a better understanding of risk implications.

In August 1995, the Commission issued a policy statement regarding the role of risk assessment in regulatory applications. The Commission indicated that the staff should be applying these techniques in all regulatory decisions to the extent that methods and data would support such applications. The Commission also has a living implementation plan that extends risk assessment approaches beyond reactors to other portions of the

nuclear regulatory program including site decommissioning, fuel cycle facilities, medical applications, and radioactive waste storage and disposal. As these risk assessment techniques are applied in regulatory decisions, it becomes critical that we understand the fundamental basis for the existing requirements. By and large, deterministic evaluations formed the basis of the existing requirements.

The approach used today is to integrate the idea of deterministic evaluations and probabilistic concepts with the end result being better safety decisions. In addition, this approach should allow more efficient use of agency resources, as well as industry resources. As budgets decrease, finding ways to improve the efficient use of resources becomes critical. Risk assessment techniques are a powerful tool to help move us in the right direction.

While the scope of the probabilistic risk assessment implementation plan is very broad, the staff is moving fairly quickly in the area of reactor applications. While we have used and continue to use risk assessment techniques in regulatory decision making, by and large the approach has been ad hoc. As a result, the implementation plan is intended to develop the necessary infrastructure for the staff to apply these risk techniques in a more systematic and consistent manner. For that reason, the Commission is moving forward on developing regulatory guides and standard review plans (SRP). While the regulatory guides will provide guidance to the industry, the SRPs are a set of documents that will provide guidance to staff reviewers as to the scope, depth, criteria and documentation requirements for acceptance of any application that relies on probabilistic techniques. There is a great deal of existing guidance that the staff has been using in a number of risk applications, not only in the U.S., but a number of documents have been developed in other countries as well on the use of risk assessment techniques.

In order to develop these regulatory guides and the SRPs, an integrated approach is necessary. There are a number of technical issues that must be addressed and the more complex issues tend to emerge from actual applications. The implementation plan identifies several pilot applications in several categories of activities selected to challenge different elements of probabilistic analysis techniques. The scope of these pilot activities varies in order to better understand the intricacies of simpler applications as well as some very detailed applications, and to anticipate questions that might arise. A number of pilot activities have been ongoing and include pilots in the areas of inservice inspection, inservice testing, technical specifications and quality assurance.

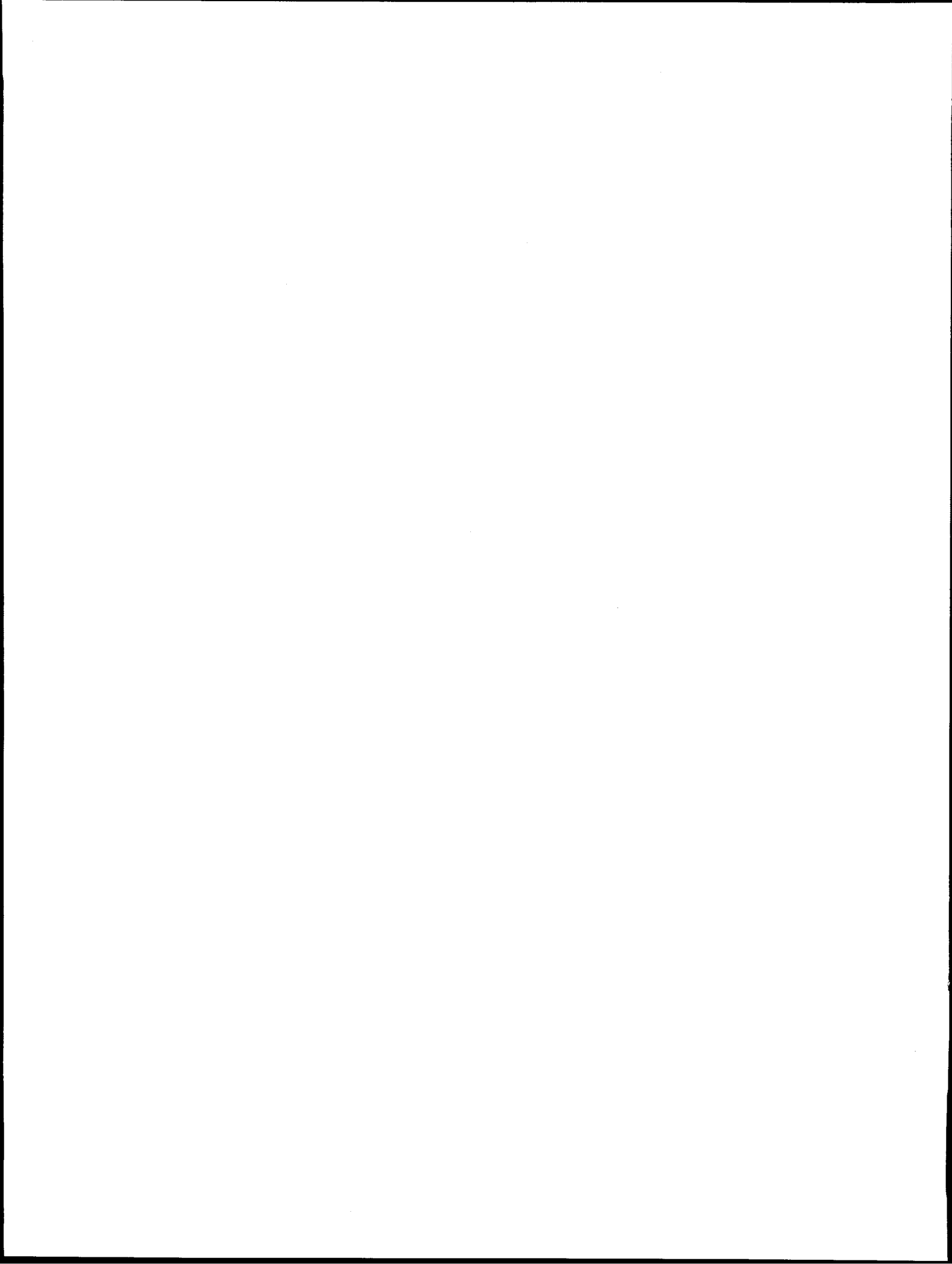
There are also several key issues for which we are looking either for some answers or for understanding as to what the limitations might be as we move forward with risk applications. Some of the issues relate to probabilistic issues such as what the scope of the study should be -- internal events, external events, Level 2, and/or Level 3? Each level introduces its own challenging issues and questions that have to be addressed. Other issues include the quality of the probabilistic study used, defining the review process, and the availability of an adequate data base.

The staff is working very hard toward meeting the schedules outlined in the implementation plan. Draft documents should be developed by the end of 1996, including a set of regulatory guides and standard review plans. Subsequent to Commission approval, the information will be provided in the public forum and the staff will be soliciting comments. These documents will form the basis for pursuing a number of risk applications.

As previously mentioned, the issue of whether or not we have an adequate data base to support these applications is critical to move forward in this area. A data base needs not only to be available, but available to the public if regulatory decisions are to be made using these techniques. The information base on which decisions are made should be scrutable.

There continues to be a need for improvement in methods. There are two important areas that need further attention. One area is the handling of common-cause failures. Although we have made improvements, we could do better. The second most important element is the development of infrastructure. We need to have adequate guidance documents, not only for the NRC staff, but also for the industry, such that the applications and the decisions would be consistent and clear. This requires focus on resources and training.

As the budget decreases, it becomes increasingly important to take a hard look at our own staff and see if we are able to train more people to be able to apply these techniques. The NRC has a fairly extensive training program in place, and we are developing the courses for our staff. Because this is clearly the direction of the future for nuclear regulation, we need to make sure that we have adequate resources to be able to address these issues. Research will continue to be a critical aspect to successfully implementing a risk-informed approach to regulatory decision making.



LATEST FINDINGS FROM THE OECD RASPLAV PROJECT

Vladimir Asmolov
Russian Research Center "Kurchatov Institute"

1. BACKGROUND

During the late phase of a severe accident in a light water reactor (current and future designs of BWRs, PWRs and VVERs), a significant amount of core material may relocate downward to the lower head of the reactor vessel. If molten core materials were to relocate to the lower head of the reactor pressure vessel (RPV), a molten pool consisting primarily of a mixture of ZrO_2 and UO_2 and some combination of a metal would form on the lower head. A solid crust of material would form around the boundaries of the pool, but internal heat generation resulting from radioactive decay of fission products would assure that most of the pool remains molten. In fact, the molten pool would undergo significant internal natural convection which would reach steady state conditions in about a few hours.

Detailed understanding of all aspects of this natural convection process, in conjunction with the thermal boundary conditions imposed on the outer surface, determines the fraction of the total heat dissipation that is transferred through the upper crust to the inside of the reactor vessel by radiative heat exchange and the fraction which must be conducted through the wall of the reactor vessel lower head. This distribution is critical in determining whether and under what conditions the molten material can be cooled and retained in the reactor pressure vessel.

Various experimental and theoretical programs have been recently performed or started in a number of countries (e.g., COPO in Finland, ACOPO in US) to address natural convection phenomena in a molten ceramic pool. However, these experimental programs are non-prototypic, especially in terms of not addressing crust formation and chemical interaction with the RPV wall.

2. OECD RASPLAV PROJECT

OECD Rasplav Project was established in 1994 as a three year program to study molten pool behavior and its interactions with structural materials in the lower head. Prior to this project in 1989 Kurchatov Institute proposed to initiate joint efforts to investigate this problem. Bilateral research agreement between US NRC and Kurchatov Institute started in 1991 was the first real step toward realization of the Project. In a set of small scale experiments behavior of coria with different materials was investigated. Data obtained during this phase provided first experience in handling of high temperature core materials in laboratory conditions. In parallel analytical analysis of related problem was initiated.

On the base of these investigations proposals to the CSNI to arrange RASPLAV project were developed and presented in 1992. Extensive discussions with CSNI experts were taken place. These discussions allowed to clarify main goals of the Project, technical approaches and Project benefits. Agreement on the OECD RASPLAV Project to Investigate Molten Reactor Fuel - Lower Pressure Vessel Head Interactions was prepared and signed by consortium of OECD countries and Russia in July 1994. These OECD sponsored joint Project brings together Russian Federation and 14 OECD countries in first nuclear safety project to be carried out in a non-OECD country. Organizational structure of the project includes:

- Management Board (MB) to control the Project;
- Programme Review Group (PRG) to act as a technical adviser to the MB;
- Operating Agent to administrate the Project in accordance with the Agreement, decisions made by MB and PRG recommendations;
- Liaison Officer to assist in the implementation of arrangements and procedures necessary for an effective supervision of the execution of the Project and exchange information.

The particular objectives of the RASPLAV Project are as follows:

- to provide data derived from integral tests on the interaction of prototypic core melt materials with the lower head of a reactor vessel, in order to assess the possibility of melt retention within the vessel;
- to perform supporting experiments and analysis to guide the integral test and develop the methodology to describe the phenomena of interest;
- to produce a consistent analysis, interpretation and understanding of the result to develop computer models, describing the phenomena.

Before start of the RASPLAV project there practically were no experimental data on properties of core mixtures containing uranium dioxide, zirconium metal and zirconium dioxide. Measurement techniques were limited at the temperatures of 2,300°C. Structural materials for the test facility itself were not developed and there was no understanding how to retain high temperature and chemically aggressive molten mass inside an experimental section under prototypic and controlled conditions. Analytical tools did not allow to provide facilities design support, pretest and post test analysis, applications to the reactor scale.

In order to cover relevant conditions the project concentrates on the investigations of prototypic melts containing uranium dioxide, zirconium dioxide and metal zirconium. Flow and heat transfer in a ceramic molten pool of melt in an RPV lower head is characterized by natural convection phenomena. This phenomenon can be almost exclusively defined by the dimensionless Rayleigh number, Ra (i.e., ratio of buoyancy to viscous forces) which is used to scale the experimental data to reactor case. In case of crust additional dimensionless numbers may appear such as Ostrogradskii number.

Characteristic Ra number for the test with the ceramic melt is of $\sim 10^{11}$ while for reactor case expected Ra number exceed 10^{15} . To cover range between RASPLAV corium test and reactor case salt experiments are being conducted where Ra more prototypic. The salt experiments will be used to extrapolate the corium test results to a Ra conditions prototypic of a full-scale reactor. The approaches being pursued will use simple correlations which are to be derived from the experimental data and incorporated into system wide severe accident codes or via the use of analytical methods which will fill the gap between the different-scale experiments and the reactor conditions.

A set of facilities was constructed and used including laboratory scale, small and large scale. Among those are RASPLAV-AW, AD, Liq, TULPAN, KORPUS, TIGEL, TEREK, RASPLAV-A-Salt. Substantial extension of material properties and interactions database was foreseen within RASPLAV Project. These data would serve to support large scale experiments and to provide data on materials interactions to be used for reactor accident analysis.

During the first year feasibility to conduct large scale test, including development of the experimental methodology, instrumentation, design materials and their protection from dangerous interactions technology was developed and tested in a set of experiments. In parallel analytical tools for analysis and interpretation of experiments has been developed. These tools include 2D and 3D codes to simulate convection/conduction problem and 2D magnetic hydrodynamic code as well developed for analysis of DEH.

Prior to conduct large scale experiment several important technical issues were solved in a series of laboratory scale and small scale experiments. The most important of them are:

- Choice of corium composition for each particular test which depends on the heating method and geometry;
- Measurements of material properties in the relevant temperature range both for solid and liquid phases;
- Development of appropriate methods of the heating up of core materials beyond liquidus temperatures ($T > 2,400^\circ\text{C}$);
- Compatibility of materials capable to withstand under extreme test conditions;
- Development of appropriate measurement techniques capable to provide necessary data at temperatures above $2,500^\circ\text{C}$;
- Development of analytical tools for pre-test and post test analysis.

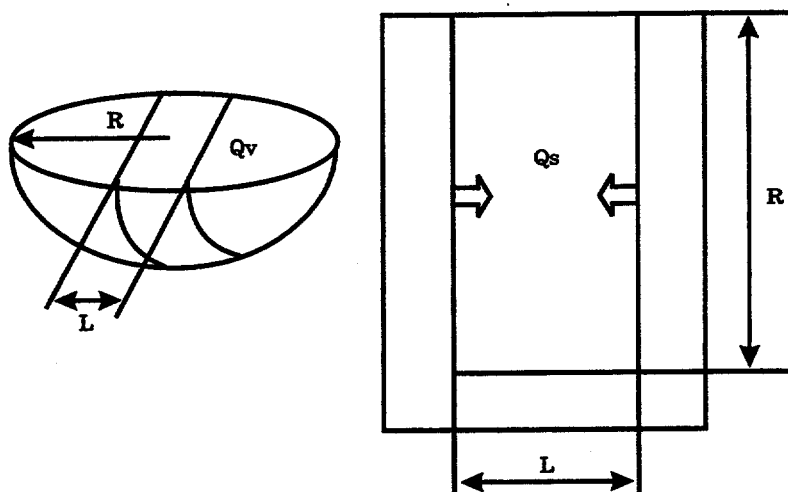


Figure 1. Test Geometry

One of the most important issues prior to the large scale experiment was choice of heating method and corresponding geometry. Two possibilities were considered: 1) to conduct experiment in a hemispherical geometry with direct electric heating (DEH) of the core materials, and 2) to conduct experiment in a slice geometry using side wall heating (SWH) or DEH methods (See Figure 1). Analytical and computational analysis of convection due to volumetric heating versus convection caused by side wall heating was completed. It was found that heat transfer to the cooled test wall for both cases seems to be similar. Direct comparison of heating methods was completed in salt experiments where similarity of heat transfer was proved experimentally.

Being realized both heating methods have inherent advantages and disadvantages. The first has similar geometry but it is very difficult to arrange uniform heating of corium mass and avoid additional Lorentz forces, which appear as a result of magnetic field generation. The second method being less prototypic needs development of the set of compatible structural materials. Two middle scale experiments were conducted to resolve this issue. The first RASPLAV-AW-1:2.5 test successively conducted in February 1995 showed the feasibility of a slice geometry and SWH. Applicability of DEH method was found to be limited due to high electrical conductivity of the corium mixture. It was demonstrated when the second test AD-1:2.5 (August, 1995) was conducted. These two tests supported by vast detailed calculations resulted in the decision to use slice geometry and side wall heating method for the first large scale test. From the AW-1:2.5 post test analysis it became clear that for successful conduction of the large scale test significant overheat of corium above liquid temperature is necessary (up to 2,700°C).

Such extreme parameters required to develop specific structural and protective materials. A number of laboratory scale experiments resulted in the development of tungsten protector, tantalum subprotector and graphite plates heated by induction. Selection of this materials for the integral test allowed to keep integrity of the test section up to 2,850°C. TULPAN facility was used for final verification of RASPLAV-AW-200 technical solutions, measurement techniques testing, and compatibility of materials in a larger scale conditions.

Several corium compositions were considered as candidates for the large scale experiments (Table 1). This coria are characterized by different properties which were measured in the liquid phase up to 3150 K. Extensive discussions taken place about

Table 1. Matrix of Corium Property Measurements

Corium	Composition, w%			Measured properties vs. temperature			
	UO ₂	ZrO ₂	Zr	Melting temperature	Conductivity	Viscosity	Thermal conductivity
C-22	81.5	5	13.5	2680 K	up to 2890 K	up to 2970 K	up to 3050 K
C-50	80	11.5	8.5	2760 K	up to 2840 K	None	None
C-100	77.8	22.2		2840 K	up to 3070 K	up to 3070 K	up to 3150 K

more prototypic corium. Corium relocated during TMI accident was mainly oxidic with negligible zirconium content (C - 100 corium). C - 22 corium containing significant amount of zirconium metal has been chosen for the first test due to less melting temperature, but difference between solidus and liquidus temperatures for C - 22 corium is 500 K, that is much higher than for C - 100 corium (about 30 - 50 K).

Along with experimental studies (Table 2) the models capable to be used for analysis proposed designs and experimental results were developed. These models simulate all the phenomena of interest such as crust formation and its dynamics, molten pool circulation, and thermal behavior of different design materials. Three dimensional approach allows to consider specific features of a design such as real geometry of the facility and heating method. Several specific models were developed one of them is model of so called "mushy" zone. Validation of codes was done using experiments with simulant materials and data obtained from AW-1:2.5 test. Results of this test were reproduced by simulation with sufficient accuracy and developed codes were extensively used during the preparations of large scale test.

Table 2. Related Experimental Studies

Tests	Objective	Composition	Corium mass, kg	Maximum temperature	Number of tests
Laboratory scale tests (Tigel, Korpus, Terek, Tulpan)	Material properties, Material interactions	C - 22 C - 50 C - 100	up to 5	3,000°C	> 100
RASPLAV-A-liq	Feasability of pouring	C - 100	12	2,900°C	13
RASPLAV-AW-2.5	Feasability of SWH	C - 22	12	2,450°C	1
RASPLAV-AD-2.5	Feasability of DEH	C - 100	40	> 2,600°C	1

Special investigation of the melt circulation up to $Ra = 10^{14}$ were conducted using the RASPLAV-A-Salt facility to support design of the large scale corium test. The properties of these salts allow to model the formation of a crust and "mushy" zone. The design of the facility provides for investigation of the effect produced by natural circulation under external cooling. Experiments with side wall heating, like in RASPLAV-AW, and comparison to heat transfer data obtained for volumetrically heated pools showed their similarity. First series of experiments with isothermal boundary conditions and with natural crust formation from the pure liquid pool by outer cooling was completed before large scale test.

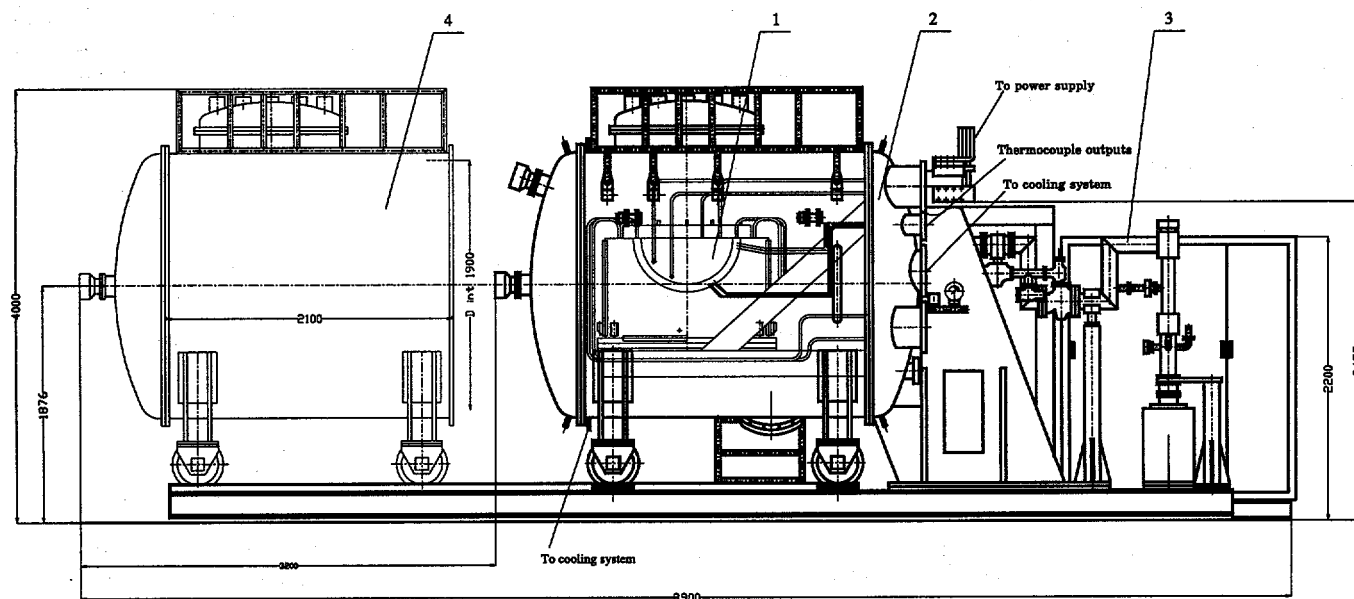
3. RASPLAV AW-200-1 TEST

After main technical issues supported design were resolved and massive pretest analysis including uncertainties showed that acceptable results may be expected a decision to conduct large scale experiment was made. Final design approach was discussed during PRG-4 meeting and approved. General view and schemes of RASPLAV AW-200 facility and test section is shown in Figures 2, 3, 4. Slice geometry with the radius of 0.4 m, 0.116 m width was used for the first experiment. Total of about 200 kg of corium was loaded in the test section (Figure 5):

- 175 kg of C - 22 corium briquettes in the form of pre-sintered briquettes with certificated properties;
- 9.3 kg of C - 100 corium to simulate crust along the vessel and to provide necessary test wall thermal insulation;
- 15.1 kg of C - 22 powder atop of loaded briquettes.

Maximum power output from thyristor converter was 300 kW at frequency 1800 Hz. Efficiency of graphite plates heating was estimated to be about 40%.

To reach on the top of the facility nearly adiabatic boundary conditions special top insulation layer made of 41 kg of C - 100 corium briquettes and powder was used (Figure 6). Tungsten-tantalum protector (1 mm thick W and 2.5 mm thick Ta sheets) was installed between corium volume and graphite heaters. Protective capabilities of the protectors were proved up to 2,850°C. Isothermal bottom boundary condition is provided by water cooling system.



Legend: 1 - Experimental section; 2 - Unmovable lid; 3 - Gas-vacuum system; 4 - Protective chamber

Figure 2. Schematic View of RASPLAV-AW-200 Facility

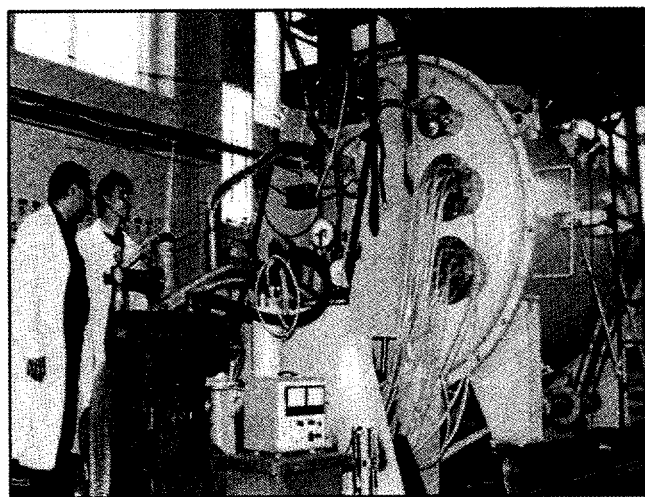
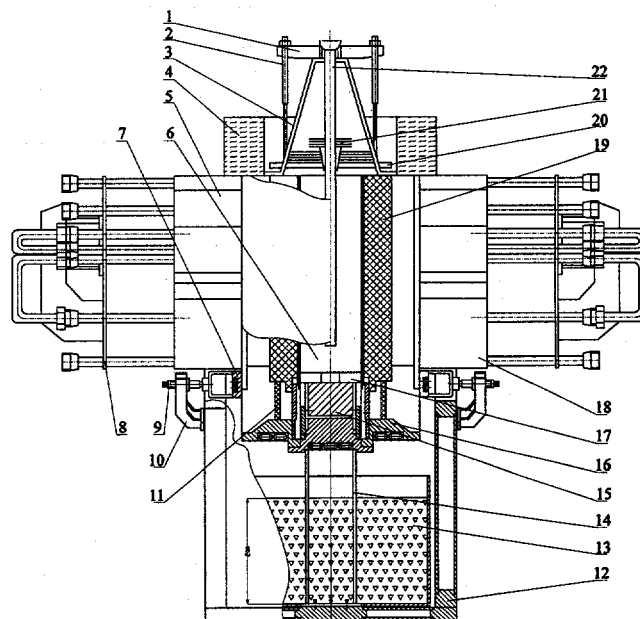


Figure 3. General View of RASPLAV-AW-200 Facility



Legend: 1 - Strap; 2 - Rod; 3 - Prop; 4 - Top lid frame (TKM); 5 - 7 - Corium C - 22; 7 - Thermal insulation presser; 8 - Base plate; 9 - Pressure screw; 10 - Bracket; 11 - Side-wall holder; 12 - Water-cooled container; 13 - Catcher; 14 - Support; 15 - Test wall; 16 - Side-wall water-cooled unit; 17 - C - 100 thermal insulation layer; 18 - Magnetic circuit; 19 - Heated side wall; 20 - Tungsten rod; 21 - Additional shield; 22 - High temperature transducer

Figure 4. Experimental Section

Data acquisition system contained about 150 channels (location of thermocouples and pyrometers is shown in Figure 7):

- pyrometers (corium and graphite plates);
- standard high temperature W - Re thermocouples;
- developed high temperature gas filled thermocouples;
- ultrasonic thermometer;
- test wall and cooling system thermocouples.

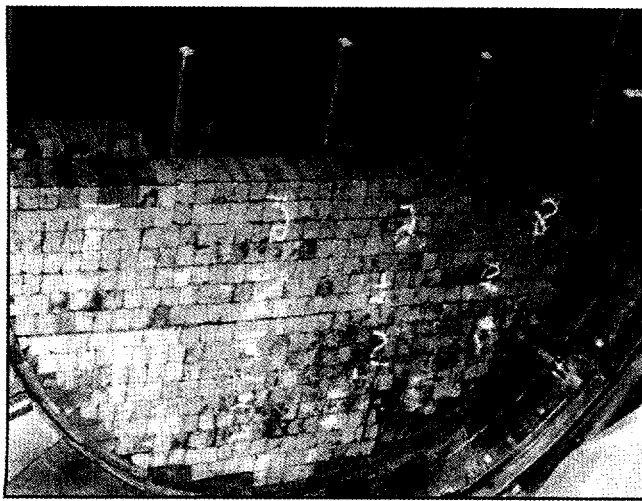


Figure 5. Charge of Test Section

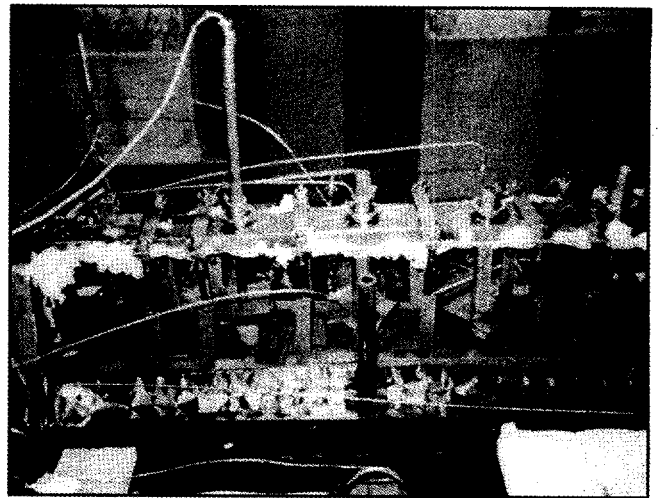


Figure 6. Top View of Experimental Section

After facility was assembled a number of dry (without corium) tests was conducted up to 2,000°C temperatures to adjust thyristor converter, magnetic leads, cooling and gas vacuum system, to determine the heating efficiency and power distribution in the graphite plates. Comprehensive pretest analysis using dry tests results allowed to increase understanding of facility operation.

The first large-scale RASPLAV-AW-200 Test was conducted October 9, 1996. The power history of the test is shown in Figure 8. During the test significant overheat of corium above liquidus temperature was reached. Maximum corium temperature was about 2,700°C and sustained above 2 hours. Temperatures measured by pyrometers installed in the corium are presented in Figure 9 (Pyr6 and Pyr5 at different locations). Test wall temperatures are presented in Figure 10.

Natural circulation in the molten corium pool was established. The test wall average heat flux was about 100 kW/m². This value was obtained based on the measured temperature gradient through the test wall and confirmed by the heat balance. Figure 11 gives the external view of the corium ingot after the side wall and protector have been removed.

Quick analysis of experimental results showed that realized test conditions were very close to pre-test predictions done by developed analytical tools which are capable to produce accurate predictions. Comparison of the test data to pretest predictions is shown in Figures 12 and 13 as a probability distribution for pre-test expectation of corium and test wall temperatures obtained as a result of uncertainties analysis. For test data uncertainties are due to experimental errors in measurements. Comparison shows very good agreement and allows to make a conclusion that during the test the behavior of the melt and structural components was fully predictable and controlled. The success criteria as it was formulated during the 4-th PRG meeting was completely satisfied.

Currently, post-test examination of the corium ingot is carried out with the aims:

- to define the shape and volume of corium liquid pool;
- to assess the molten corium fraction;
- to define phase and element distribution within the ingot to evaluate the possibility of stratification caused by gravitation and temperature fields;
- to investigate of crust near surface layers and liquid-solid interface;
- to investigate test wall-corium interaction;
- to assess temperature distribution in the test facility.

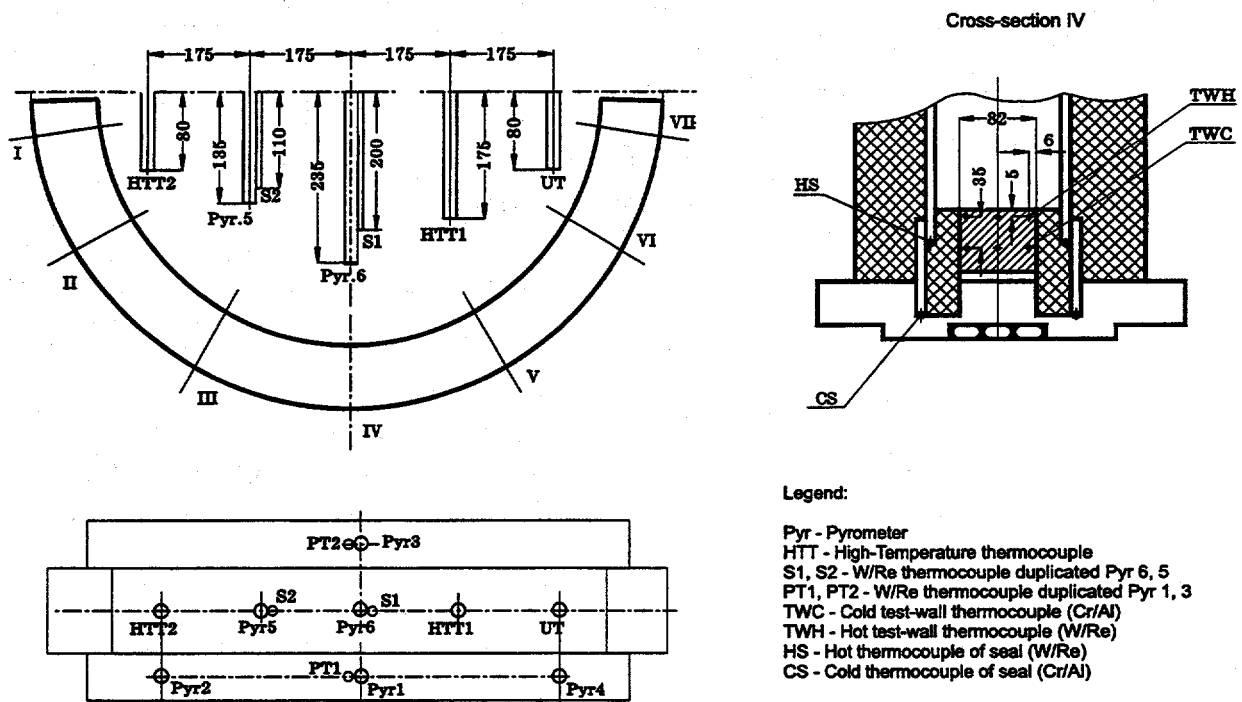


Figure 7. Location of Main Temperature Measurement Points

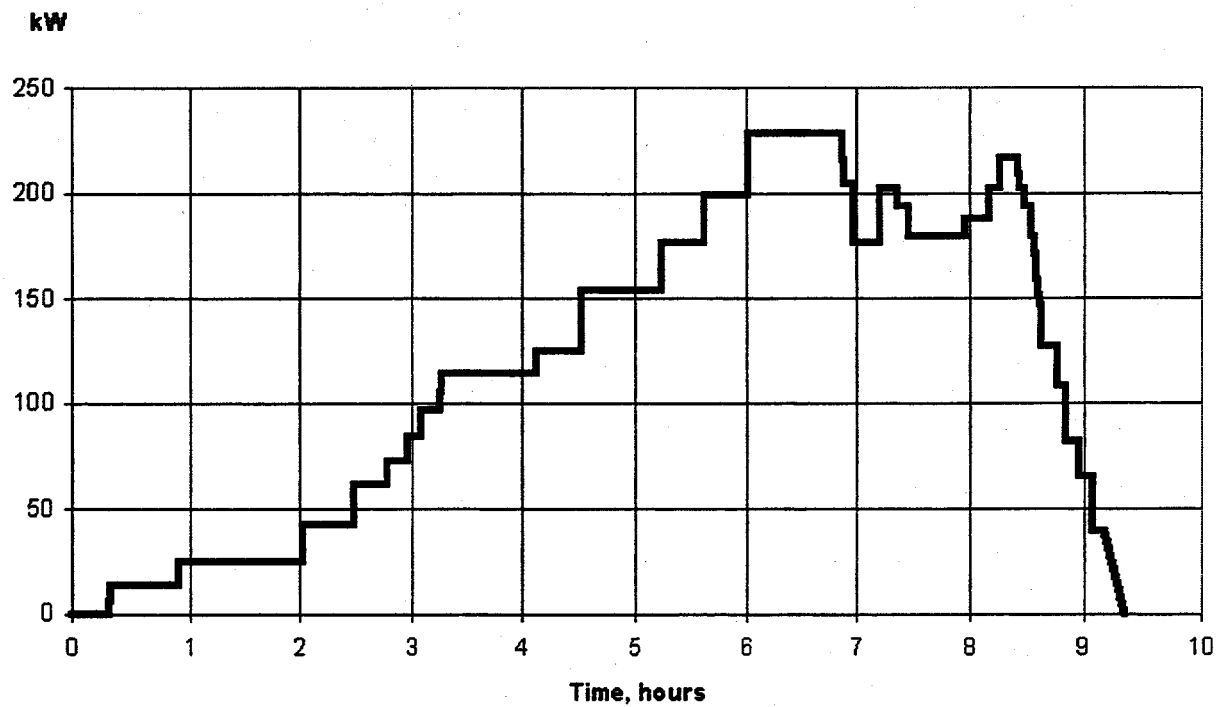


Figure 8. Power History

T, °C

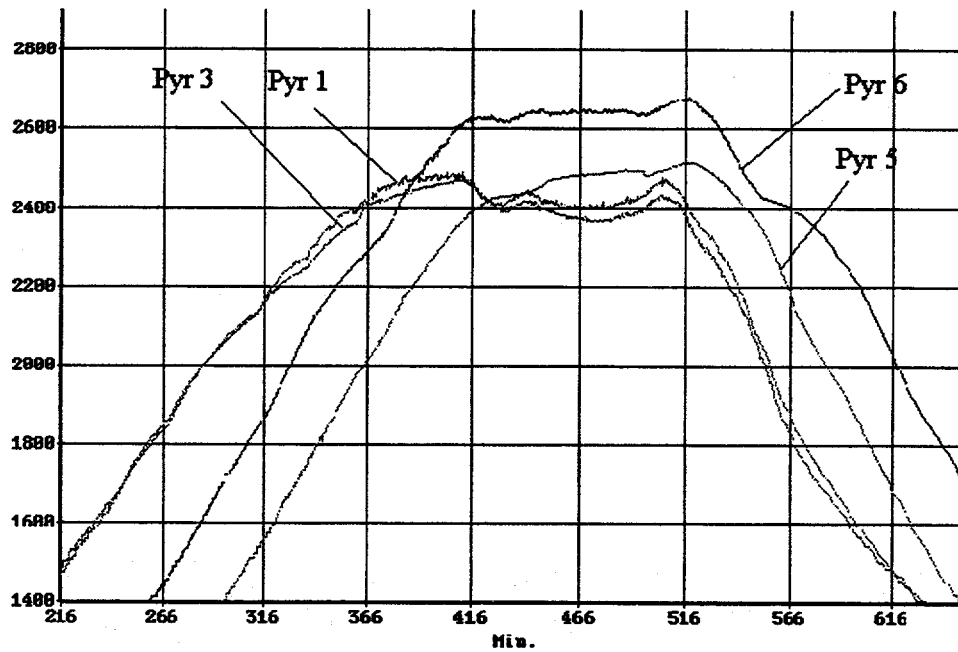


Figure 9. Pyrometer Measurements

T, °C

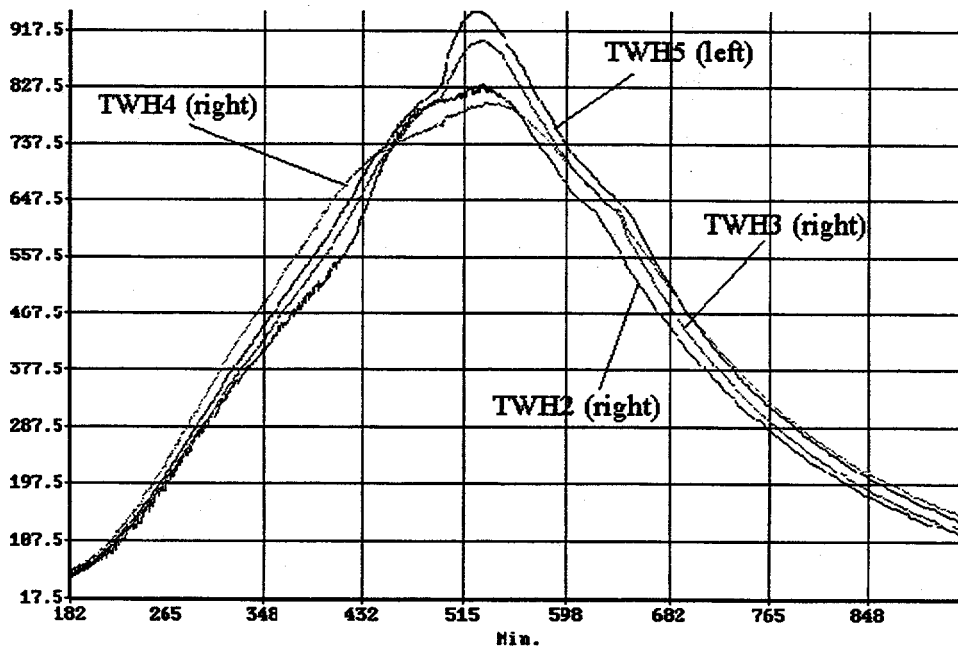


Figure 10. Test Wall Temperatures

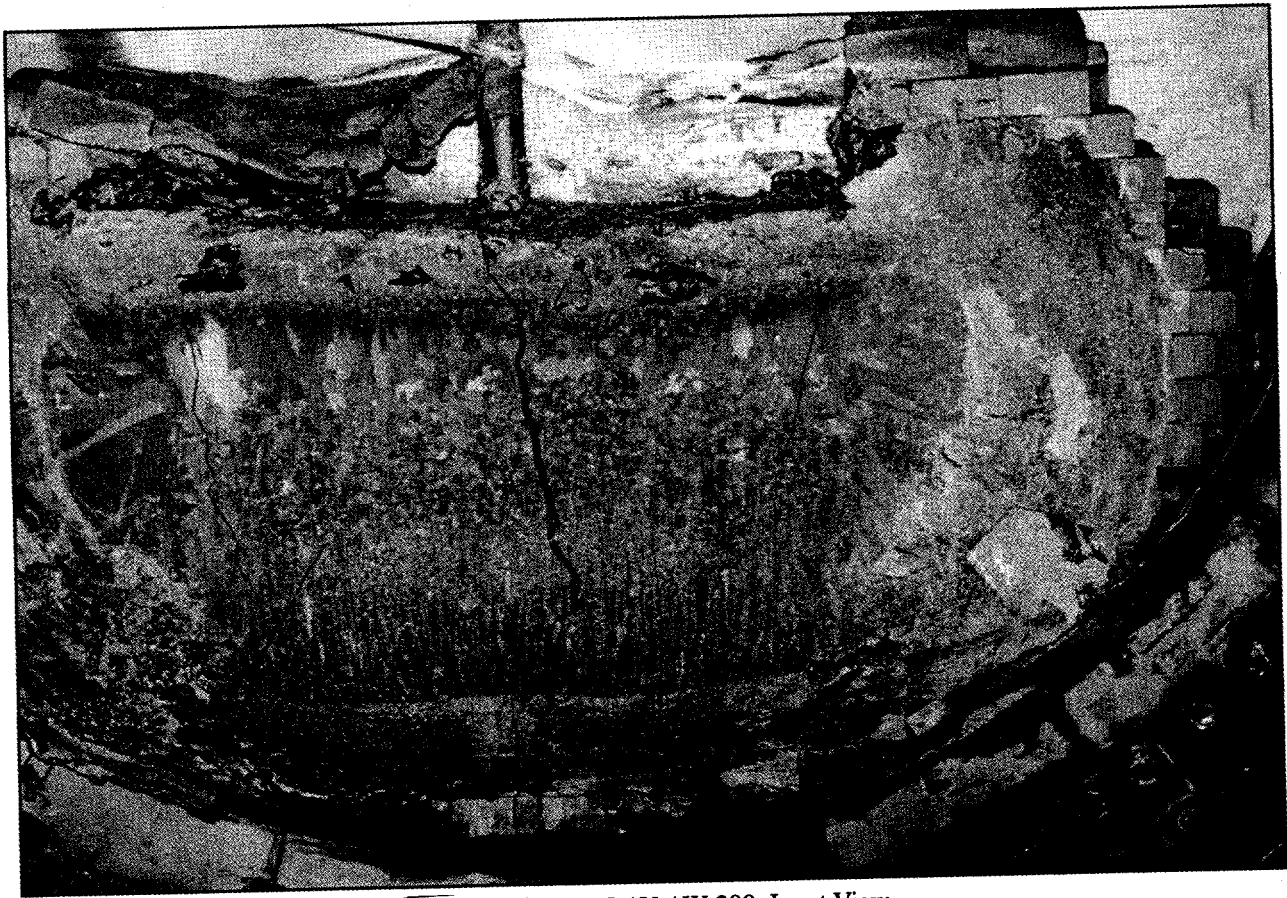


Figure 11. RASPLAV-AW-200: Ingot View

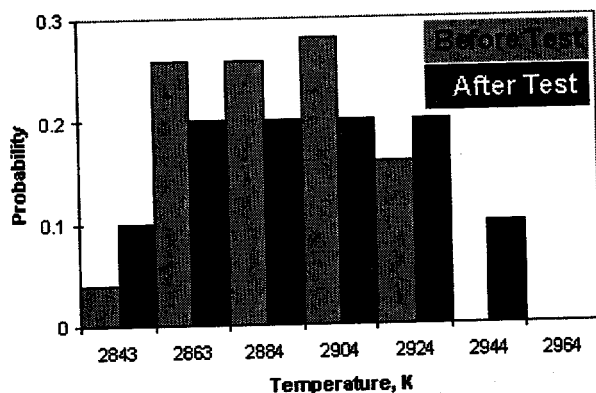


Figure 12. Probability Distribution of Corium Temperature

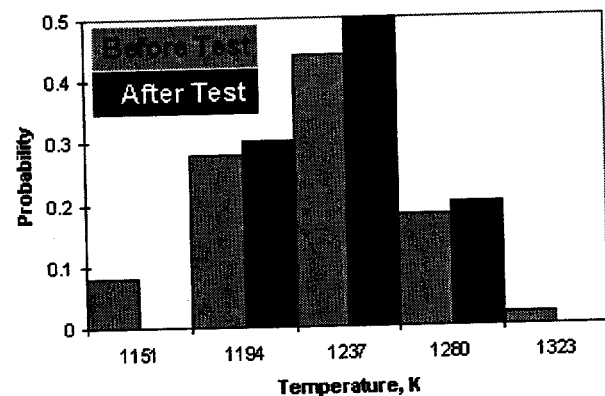


Figure 13. Probability Distribution of Test-Wall Temperature

4. CONCLUSIONS

The first large scale AW-200 test was conducted with prototypic core materials and expected results of post test examination provide a very important step to understand and analyse in-vessel molten pool behavior. Controlled heat-up phase, controlled sustaining in quasi-steady-state condition during more then 2 hours and controlled cooling down phase have revealed the possibility to provide appropriate knowledge base to handle molten core materials in the reactor pressure vessel lower head.

Extensive experimental base have been created within RASPLAV Project to investigate corium behaviour in sufficient large melt pool. Capability of developed design solution, materials and measurement detectors was demonstrated. Involved staff gained considerable knowledge and skills in field of corium studies in control accidental condition.

The Role of Research in Nuclear Regulation

Opening Remarks

James M. Taylor
U. S. Nuclear Regulatory Commission

Good morning, ladies and gentlemen. My name is James Taylor. I'm the Executive Director for Operations of the U.S. Nuclear Regulatory Commission. I'm very pleased to join you at this meeting. And, to all of you from outside NRC, I give a special welcome. I appreciate the work of the staff to put on this important conference every year.

More than 20 years ago, the Energy Reorganization Act of 1974 created the USNRC and that same act provided for an office of nuclear regulatory research. It's what we call a statutory office within the NRC. In providing for an NRC research program, our Congress had several things to say about the character of the research that would be performed. I'd like now to just briefly describe what was intended by Congress in creating this statutory office.

First, NRC should perform such research as is necessary for the effective performance of the Commission's licensing and related regulatory functions.

Second, the research may be characterized as confirmatory reassessment related to the safe operation and the protection of commercial reactors and other nuclear materials.

Third, the NRC should have an independent capability for developing and analyzing technical information related to reactor safety, safeguards, and environmental protection in support of both the licensing and regulatory processes.

Fourth, the research should not go beyond the need for confirmatory assessment, because the NRC should never be placed in a position of having generated and then having to defend basic design data of its own.

This has been and continues to be the role of research at the NRC. Somewhat different purposes might apply for regulatory agencies in other countries. As you know, several countries' regulatory agencies are represented here on this panel, so we may hear about some of these differences. Other panel members from research and industry organizations may have other views about why or if a regulatory agency should perform research and what the role of that research should be.

Each of the panel members has agreed to make about a 10 to 15 minute presentation of their views on the role of research in nuclear regulation. Following that, we have allotted time for discussion among the panel members and for questions to be taken from the floor.

The Role of Research in Nuclear Regulation An NRC Perspective

David L. Morrison
U. S. Nuclear Regulatory Commission

1. BACKGROUND

The role of research in the US Nuclear Regulatory Commission was broadly defined by the US Congress in the Energy Reorganization Act of 1975. This Act empowered the Commission to do research that it deems necessary for the performance of its licensing and regulatory functions. Congress cited a need for an independent capability that would support the licensing and regulatory process through the development and analysis of technical information related to reactor safety, safeguards and environmental protection. Motivation for establishing such a safety research function within the regulatory agency is the need to address the defects, abnormal occurrences and shutdowns involving light water reactors. Congress further stated that the NRC should limit its research to "confirmatory assessment" and that the Agency "should never be placed in a position to generate, and then have to defend, basic design data of its own."

The role of research within the USNRC has additional roots pursuant to the safety culture that the Agency embraces. In 1986 the Commission established two safety goals. First, individual members of the public should be provided a level of protection from the consequences of nuclear power plant operations such that individuals will bear no significant additional risk to life and health. Second, societal risks from nuclear power plant operations should be comparable to or less than the risks of generating electricity by viable competing technologies and should not make significant additions to other societal risks.

Traditionally, regulation of the design and operation of commercial nuclear power plants has been based on various deterministic criteria. These criteria required a licensee to assure that the plant could be placed in a safe condition following a number of postulated design basis accidents. Since minimal operating experience or test data existed when these criteria were established, both the postulated accidents and the techniques used to evaluate the plant's response were established to be intentionally conservative; that is, conservative safety margins were established. Deterministic criteria also provided the basis for identifying what plant structures, systems, components (SSCs), and activities were important to safety in order to ensure defense-in-depth. These SSCs and activities were controlled through regulatory requirements.

During the last several years, both the NRC and the nuclear industry have recognized that Probabilistic Risk Assessment (PRA) has evolved to the point where it can be used as a tool in regulatory decisionmaking. In 1995, the NRC adopted a policy that the use of PRA technology should be increased in all regulatory matters in a manner that complements the NRC's deterministic approach and supports NRC's traditional defense-in-depth philosophy. PRA and associated analyses (e.g., sensitivity studies, uncertainty analyses and importance measures) are to be used in regulatory matters, where practical within the bounds of the state-of-the-art, to reduce unnecessary conservatism associated with current regulatory requirements, regulatory guides, license commitments and staff practices. These PRA evaluations in support of regulatory decisions should be as realistic as practicable and appropriate supporting data should be publicly available for review. In addition, the Commission's safety goals for nuclear power plants and subsidiary numerical objectives are to be used with appropriate consideration of uncertainties in making regulatory judgements on the need for proposing and backfitting new generic requirements on nuclear power plant licensees.

2. USNRC REGULATORY RESEARCH

The statutory mandates, the safety goals and the PRA policy statement clearly establish the basis for the NRC's regulatory research program. For convenience and brevity, I'll discuss the research program along three dimensions: the scope of the research; the origination of the research requirements; and the purpose of or motivation for the projects.

Within the USNRC the scope of research is generally characterized as confirmatory or exploratory. Confirmatory research is initiated in response to a need expressed by the user or program office (i.e., NRR, NMSS, or AEOD) and is undertaken to provide direct support to a regulatory activity (eg., review and approval of licensee applications, burden relief and generic backfitting, rulemaking, inspections, and regulatory guidance). Confirmatory research is generally design basis oriented, and the products of confirmatory research include a comprehensive understanding of the relevant phenomena and systems, validation of experimental data, and the development of analytical techniques and computer models. Approximately 80% of NRC's research budget is spent for confirmatory research.

Exploratory research is initiated within the Office of Nuclear Regulatory Research for several purposes. While current regulatory needs can usually be satisfied by focusing on design basis events, the assurance of public health and safety demands examination of mechanisms and phenomena well beyond design bases. The assessment and assurance of margins both in design and throughout long term operation requires ongoing research activities to identify new phenomena that may arise due to aging, to anticipate and provide the technical basis to address new issues as they arise, and to respond to and incorporate knowledge from advances in technology into the Agency's regulatory activities.

The third dimension of regulatory research is to maintain and acquire the technical skills and technical bases used by the Agency to conduct its regulatory mission. One of the roles of the Office of Nuclear Regulatory Research is to maintain the corporate memory of the Agency's technology. Support of and participation in active, ongoing research projects, both in the United States and internationally, are important means whereby this role is fulfilled. In addition the staff of the Office is challenged by management to maintain awareness of developments in relevant technical fields and to acquire these skills for the NRC.

3. PAST RESEARCH ACCOMPLISHMENTS

In the past NRC's research has focused largely on (a) confirming the technical bases for regulatory judgements underlying an increasingly comprehensive body of rules and staff guidance, (b) resolving safety issues arising in part from operating experience, seeking generic regulatory solutions, and (c) focusing on safety concerns with great uncertainty, such as those associated with equipment performance and life, and with severe accidents. Major themes that had their origins in the past have evolved into our current and continuing programs. For example, the thermal hydraulics program that had its origins in studies initiated in the 1970's to confirm and quantify the margin of conservatism in the ECCS rule. These studies culminated in a revised ECCS rule in 1988. A severe accident research program initiated in 1980 provided a sufficient understanding of the phenomena involved in severe accident progression that led the Commission to conclude in its 1985 Severe Accident Policy Statement that current plants posed no undue risk to public health and safety and to withdraw its Advanced Notice of Proposed Rulemaking. In this policy statement, the Commission formulated an approach for a systematic safety examination of existing plants to study particular accident vulnerabilities and to examine cost-effective changes to ensure that there is no undue risk to public health and safety. Generic Letter 88-20, in November 1988, requested all licensees to perform an Individual Plant Examination to identify any plant-specific vulnerabilities to severe accidents, and to report the results to the Commission. Based upon their IPE's, a number of utilities have modified hardware and procedures to further reduce vulnerabilities.

4. CURRENT DIRECTIONS

Our current TH program is directed at understanding the underlying phenomena of the advanced, passive designs and the maintenance and improvement of computer codes for operating and transient analyses. We are also maintaining international collaboration on severe accident research to gain further understanding of the phenomena involved; performing research on the aging of vital reactor components, systems and structures to support continued operation of existing power plants and to provide a sound technical basis for license renewal; and reviewing and extracting insights on risks of power plant operations from the PRAs that have been submitted by the licensees in response to a request by the Commission.

5. FUTURE DIRECTIONS

While the relative emphasis of the topics encompassed by NRC's research program will shift somewhat in the future, the role of research in support nuclear reactor regulation will remain the same, that is, to assure that the best available knowledge from research forms a basis for maintaining risks at an acceptably low level. As operating plants continue to age, the assurance of margins is a major focus of our research program. The understanding of underlying phenomena of materials behavior, the availability and use of valid analytical techniques and models, further improvements in techniques to test and monitor existing conditions, and the performance of testing beyond normal operating conditions are collectively the means to establish such assurance of margins. These are issues of international concern, and research areas that have enjoyed a long history of fruitful international collaboration.

6. CLOSURE

I want to close with a brief remark on the products of regulatory research. NRC's public health and safety mission demands that its research products be developed independently from its licensees; be credible and of the highest technical quality as established through peer review; and open to the public scrutiny through publication in technical journals as well as NRC documents. A special trust is placed on regulatory research through the products it produces as well as the three dimensions that underlie the processes through which they are produced.

The Role of Research in Nuclear Regulation

A French Perspective

Michel Livolant
Nuclear Protection and Safety Institute
Atomic Energy Commission (France)

I thank NRC for inviting me to present some of our views about the relationship between research and regulation in the safety domain. And first, to introduce my talk by putting it on a sound basis, I will give some words about our nuclear institute.

Roughly speaking, our role is similar in the French situation to the NRC administration role but with less authority role, which corresponds to another body in France. We define ourselves as a technical support of the safety authorities. On the other hand, we have our own research laboratories. Among them, the most famous are the Phebus reactor and the Cabri reactor about which we have heard a lot these two days.

So the work we do is summarized in this slide. You can see that we work on safety but also on protection of man and environment, management of accident conditions, security of transport, and safeguards.

The next slide shows the position we have in the French system. We are in the circle here as research assessment and evaluation. What is important to notice is that we have by function, a relationship with utilities on the one hand and with government authorities on the other hand.

Utilities for us means mainly EdF or power plants but also CEA for research installations and hot labs and Cogema for reprocessing plants. And with those utilities we have technical evaluation of two types. We make detailed technical studies of the safety reports presented to the authorities by the utility. This is an evaluation-type work for the authority. But, on the research side, we make common research program to resolve issues or to increase knowledge and understanding about questions that could correspond to some improvement in safety. So, in that case, we have already the sort of common research program with utilities that was discussed before.

With authorities, our role is to give advice on safety reports of existing or being-built installations and on more general policy questions like, for example, the safety principle to apply to the next generation of power plants. The decision is left to the safety authorities, but we give a lot of advice and detailed studies about that. In some cases, we can also advise them on some possible issues in the future.

I have taken some time to present this organization to show the relational system on which we rely. The high level of relations at technical level between all the parties explains why the formal character of regulation by text is less developed in France than in the USA or other countries. For that reason, later in my talk, I will use the word regulation as the principle and practice used in the safety evaluation and acceptance process. That's not strictly text but a general process to accept with the operation of plants.

Rather than speaking in general terms on research and regulation, I will now give some examples of different domains and present the actual objective of research and regulations.

The first point is thermal hydraulics. We'll have some word about that. It's clear that this domain is really a domain where a lot of work has been done and there are existing codes that are very good, such as RELAP, CATHARE, ATHLET and some other codes, all are very good.

So, the research now is to focus on other topics that are explained here, configuration of uncertainties, physical and numerical modeling, nodalization, user effects, extension of the simulation domain to those that are not so well covered or validated by the present type of codes, start-up and cool-down transients, low power and shutdown modes, accident management procedures, and, something very difficult, 2D and 3D computational models. Also there are some improvements needed for new reactors. So, clearly we are not to start from zero in thermo-hydraulics. We have a very strong base, but, nevertheless, there are still new improvements.

For regulation objectives, it's clear that there is a need today to validate methods for transient behavior calculation and to replace some codes in the domain where there are still conservative methods, that is to replace them with methods based on best-estimate calculations, including real good evaluation of uncertainties. Next slide, please.

One more comment before leaving the slide about the thermo-hydraulic domain. We believe that in this domain, thus far, we do not need a formal change in regulation. But regulatory people can reach the point of acceptance of improved techniques to make a safety demonstration with the advantage for the safety authorities to have a better appreciation of the risk, and here we slowly get to the risk in the form of regulation even without changing strictly the regulation, and, for the utility of this, we may have the advantage of more flexible demonstrations and ability to optimize design solutions.

Here we have another topic that is of interest in discussion now: high burnup fuel. The question is: are the rules used for fresh or low burnup fuels still valid for high burnup fuel? The research is mainly done by in-pile tests but also by hot lab tests and mechanical behavior of cladding or corrosion tests, and the key parameters in the cladding corrosion with a validity of physical phenomena that are very complex and were explained largely on Monday: oxidation, spallation, hydriding.

For the future, there is some concern with the behavior of the external fuel layer that may have some role in some accident conditions. Here the regulation objectives are clear. We need validated rules in the burnup range, so there is a need for a change in regulation because of this accident. It's a really straightforward domain where the relationship between research and regulation is straightforward. Following the results of research, the rules for demonstration of acceptance of high burnup fuel have to be adapted. This is a view of one of the tests which shows a risk of rupture of cladding at relatively low energy deposition.

This third topic concerned the severe accident and the source term. Those figures are true for the future plant in the French view actually. There is an idea that for the future plant we need to reduce the level of source term to a very low level so that the population need not be evacuated or, at least, at a very limited level. Some research is needed to be able to prove that. A large part of the research is done in the Phebus Fission Product program, and the regulation objectives are to arrive to validity in methods for the source term evaluation.

Clearly, in this third domain the relationship is less direct between the research and regulation. But this is demonstrated by a crisis exercise, and, as required in France by safety authority, we have to improve that iodine method to reach this source term.

To conclude my presentation, I can say that it's my personal conviction that research is one of the tools we have to improve safety through appropriate transfer to regulation, which is not so easy to do. In whatever country we have to improve the relationship between research and regulation, and this you cannot do one day and say okay it's done. We have to do that and continue to insist on that.

The other tool for improving regulation is naturally the operating experience that was largely mentioned before and is considered also very important. But it's clear that regulation implies other considerations than

those coming from research achievements. However, the importance of the research achievements should be recognized.

It is also my conviction that regulators have to accept the fact that the rules do not necessarily have to become more severe if solid technical evidence exists to make them more flexible without reducing the safety objectives. This certainly is a rarity now because people like very well the rules they have set up and don't like to change them, but I understand that the line of risk-informed performance-based regulation which was discussed on Monday will set a new road to go in the right direction even if the road will be certainly long.

NUCLEAR PROTECTION AND SAFETY INSTITUTE

NUCLEAR PROTECTION AND SAFETY INSTITUTE

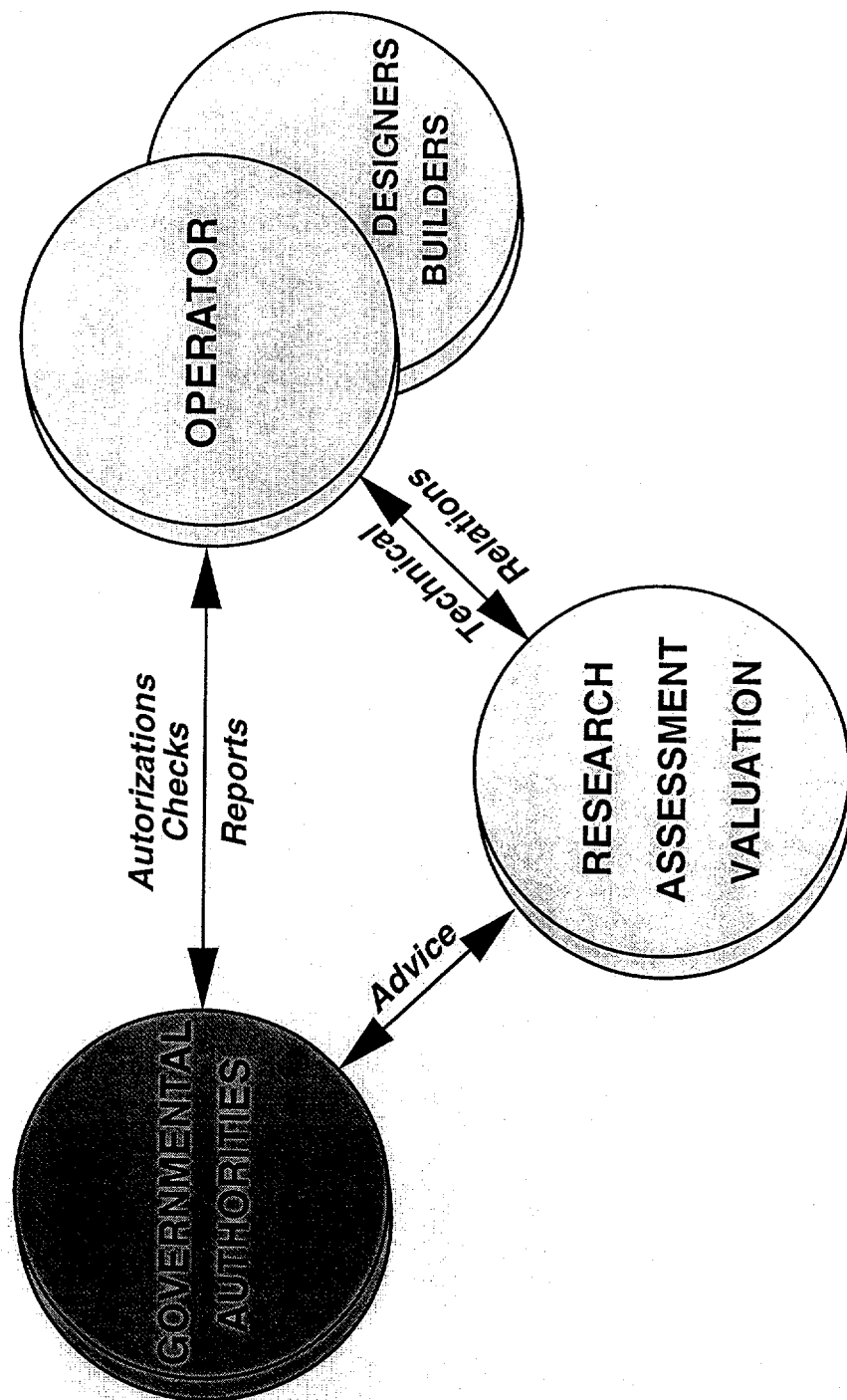


GENERAL TASK

**RESEARCH AND EXPERT EVALUATIONS IN ALL THE FIELDS OF
STUDY NECESSARY TO THE CONTROL OF NUCLEAR RISKS :**

- **SAFETY OF INSTALLATIONS,**
- **PROTECTION OF MAN AND OF THE ENVIRONMENT,**
- **MANAGEMENT OF ACCIDENT CONDITIONS,**
- **SECURITY OF TRANSPORTS**

THE IPSN'S ROLE IN GENERAL NUCLEAR SAFETY IN FRANCE



Thermal Hydraulics

Research:

- ⇒ Quantification of uncertainties
 - . Physical and numerical modelling, nodalisation, user effects..
- ⇒ Extension of the simulation domain
 - . Start-up and cool-down transients, low power and shutdown modes
 - . Accident management procedures
 - . 2D/3D computational models

⇒ New Reactors

Regulation objectives:

- ⇒ Validated methods for transient behaviour calculations with best estimates codes in the overall range of situations, including uncertainties evaluation

High burnup fuel in accident conditions

RIA

Are the rules used for fresh or low burnup fuel still valid for high burnup fuel?

Research: In pile tests CABRI, NSRR

⇒ The key parameter is the cladding corrosion:
. Oxidation, spallation, hydruration

⇒ The behaviour of the external fuel layer (RIM) needs further studies

Regulation objective:

⇒ Validated rules in the high burnup range

Severe accidents

Source term

For future plants, less than 1/10000 of the iodine and cesium should escape from the reactor in case of a severe accident

Research: PHEBUS-FP

- ⇒ Core degradation
- ⇒ Fission products emission as function of core degradation
- ⇒ transfer through the primary circuit
- ⇒ Deposition in the containment

Regulation objective

- ⇒ Validated data and methods for source term evaluation

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The Role of Research in Nuclear Regulation

Status and Future Activities in Japan

K. Soda

Japan Atomic Energy Research Institute

The Role of Nuclear Regulation

The role of nuclear regulation is grouped into the three categories in the Basic Safety Principles for Nuclear Power Plants, the INSAG-3 document of IAEA published in 1988. First category is to specify and develop standards and regulations for safety, and to issue licenses to operating organization. Second category is to inspect, monitor and review the safety performances of nuclear power plants and operating organizations. In the second category, corrective action may be ordered if it is found necessary after inspection, monitoring and review. The third category is to advocate safety research and disseminate safety information. Nuclear safety research is closely related to nuclear regulation.

Status of Nuclear Regulation and Research

The licensing procedures of nuclear facilities require two steps approach in Japan, that is, those who wish to construct and operate a nuclear plant must apply for a government approval for construction and operation. Safety examination is then performed first by the government, and the second examination is carried out by the Nuclear Safety Commission (NSC). In this process, research information is supplied to the Advisory Committee on Technical Matters (ACTM) which is under the Ministry of Trade and Industry (MITI) and to the Committee on Examination of Reactor Safety (CERS) which is under the Science and Technology Agency (STA). Research organizations are asked by those Committees to provide data needed for safety examination and to perform safety analyses for verification of analyses submitted to the Committees by the licensees.

In addition in the licensing procedures, examination guides needed for the safety examination are based on experimental data and analyses performed by research organizations by the government request.

Research Reflected in Nuclear Regulation

Some examples of research reflected in nuclear regulation in the past years in Japan are as follows. Design criteria for reactivity initiated accident (RIA) are based on the experimental

data obtained at NSRR of JAERI. Experimental data of JAERI's LSTF and CCTF give basis of the ECCS performance criteria. Large scale experiments are conducted for safety demonstration of nuclear reactors by the special fund from the government; those include the 2D-3D program by using the CCTF facility, seismic tests at the large scale shaking table of NUPEC at Tadotsu and hydrogen experiments by NUPEC. Investigation of accidents in the past such as TMI-2, Chernobyl, Mihama and Monju has been carried out and often resulted in lessons for further improving safety.

Nuclear Safety Research

Among several roles of research in nuclear regulation, nuclear safety research is the most important to research organizations. Nuclear safety research in Japan is conducted in accordance with the National Nuclear Safety Program (NNSP) which was first established in 1976 to ensure safety of nuclear facilities. NSPP has been revised every five years to take into account of expansion and diversification of nuclear development as well as new lessons learned from new experiences gained from normal operation and abnormal occurrences if any.

Major Safety Issues

NNSP covers important nuclear safety research sponsored by the government in the area of safety on nuclear facilities, environmental safety and radioactive waste management. The current NNSP is for the five year period of 1996 to 2000 with emphasis on six major safety issues, namely RIA for high burnup fuel, improvement of accident management, aging of plant, seismic design and PSA, human factors and reliability of digital system. Research organizations utilize their facilities and expertise to meet regulatory needs.

Summary and Future Activities

Nuclear research plays the essential role in nuclear regulation especially in the area of safety research. In this regard, nuclear research is incorporated in licensing procedures and nuclear safety research is carried out in accordance with the national nuclear safety program for nuclear facilities in Japan. Nuclear research plays an important role in achieving public understanding and acceptance to use of nuclear energy for power production. International collaboration is also important to further improvement of safety of nuclear facilities around the world by considering the fact that impact of any reactor accident in any country may influence other country's regulation.

The Role of Research in Nuclear Regulation Status and Future Activities in Japan

K. Soda
Japan Atomic Energy Research Institute

Presented at the 24th WRSN, Bethesda, MD
October 23, 1996

The Role of Nuclear Regulation

- To specify and develop standards and regulations for safety, and to issue licenses to operating organizations,
- To inspect, monitor and review the safety performance of nuclear power plants and operating organizations, and
- To advocate safety research and disseminate safety information.

(from IAEA-INSAG-3, Basic Safety Principles for Nuclear Power Plants)

Research Reflected in Nuclear Regulation

- Establishment of Examination Guides
 - RIA design criteria- NSRR, ECCS experiments- LSTF/ CCTF, Suppression system tests.
- Safety Demonstration Experiments
 - 2D/3D experiments- CCTF, Seismic tests- NUPEC/Tadotsu, Hydrogen tests- NUPEC
- Investigation of Accidents
 - TMI- 2, Chernobyl, Mihama, Monju

Contents

- The Role of Nuclear Regulation
- Status of Nuclear Regulation and Research
- Research Reflected in Nuclear Regulation
- Nuclear Safety Research and Major Safety Issues
- Summary and Future Activities

Status of Nuclear Regulation and Research

- Licensing procedures of nuclear facilities require safety examination by the regulatory body and examination by the Nuclear Safety Commission (NSC).
- Research information is supplied to the Advisory Committee on Technical Matters (ACTM) and the Committee on Examination of Reactor Safety (CERS).

Nuclear Safety Research

- National Nuclear Safety Program (NNSP) was established in 1976 to ensure safety, and revised every five years to take into account of expansion and diversification of nuclear development.
- NNSP covers important nuclear safety research sponsored by the government in the area of :
 - Safety on nuclear facilities,
 - Environmental safety, and
 - Radioactive waste management.

Major Safety Issues

The current NNSP (1996- 2000) emphasizes
six major safety issues:

- RIA for high burnup fuel
- Implementation of accident management
- Aging of plant
- Seismic design and PSA
- Human factors
- Reliability of digital system

Summary and Future Activities

- Nuclear research is incorporated in licensing procedures.
- Nuclear safety research will be carried out in accordance with NNSP for nuclear facilities.
- Nuclear research is essential to achieve public understanding and acceptance to use of nuclear energy for power production.
- International collaboration is important to further improve safety of nuclear facilities around the world.

The Role of Research in Nuclear Regulation A U.S. Industry Perspective

Ronald L. Simard
Nuclear Energy Institute

Listening to Mr. Taylor, it strikes me that this could have been a very short and uninteresting session if we had met some 24 years ago at the first meeting, because I think at that time we all would have agreed on what the role of research in regulation is. Certainly here in the States, at that time the NRC research programs were clearly focused on research in support of rulemaking and generic-issue resolution largely to support the licensing of U.S. plants that was going on at the time, and the focus was on design basis accidents. This was an appropriate focus given our limited experience base because at that time we had only some 200 reactor years of operating experience.

But, as we went into the 1980s, after the accident at TMI, I think we clearly would have agreed that there was a need for a better understanding of beyond design basis events: severe accidents leading to core damage. And, as the U.S. plants matured, we needed a better understanding and an ability to predict how structures and components would age. Also by the end of the 1980s, we had accumulated some 1400 reactor years of operating experience in U.S. plants. So, I think it would have been pretty clear at that time what the focus of NRC research ought to be and what its role was. At the end of the 1980s, as we went into this decade, NRC research responded -- I think we would have said appropriately -- to emerging technologies and work on advanced plant designs.

So looking back in time, I'd think we'd agree there's no question NRC research has made a significant contribution in terms of providing a sound technical basis for creating new regulations or modifying existing regulations, and I think we could easily agree on a list of areas in which the research programs of the NRC have contributed.

But if we look forward in time, I mean, just look to next year. Look at the milestones that are ahead of us next year. Next year will be the 25th Safety Information Meeting. It will be the 40th anniversary of nuclear generation in the United States, and our experience base will be up to 2500 reactor years of operating experience with U.S. plants. The technology is mature, and by all indicators, both the industry indicators and the NRC indicators, plant safety and reliability have improved significantly.

We can never afford to become complacent, of course, but we've obviously reached a point where there are fewer unresolved safety issues, and our analytical tools have improved to the point where the value of further improvements is now being questioned.

It's time to reexamine the role of NRC research in the regulation of U.S. nuclear plants, and the pressure is on us to do it quickly. Last week, Chairman Jackson said that the NRC operates in a world of changing responsibilities, changing legislation, changing budgets, changing market forces, and changing stakeholder expectations. And I think over the past couple of days you've seen plenty of specific examples of what that means to the NRC staff in responding to the urgent need to reevaluate their research programs under increasing budget constraints.

So I thought what we might do today is pose a few questions as to how the NRC might go about doing this. How can they respond to these very real pressures in a way that doesn't compromise the value or the integrity of NRC research results?

Monday's panel used a neat trick. When you don't have all the answers, you put everything in the form of questions. That worked pretty well Monday, and maybe this morning we could look at this challenge and ask what are probably some fairly common sense questions.

For example, if you look at proposed new research, what criteria might be used to evaluate proposed new research? For example, is the product going to produce a well-defined work product and can it be tied, at least qualitatively if not quantitatively, to protection of public health and safety? Is there a well-defined end use for the work product, and is it really necessary to support a regulatory decision? Is it necessary for NRC to do this research, or are there similar programs in place at EPRI or overseas that can be leveraged through increased cooperation? Or, if this proposed new research falls into the category of confirmatory research that Mr. Taylor mentioned and it's supposed to confirm an industry analysis or experiment, has an attempt been made to ensure consistency of data collection in order to minimize conflicting results?

Now that last question has come up before. I know there's been a lot of discussion about NRC independence, and I think, again, another area of clear agreement among us in this room and between the NRC and its licensees is that NRC research needs to remain independent, certainly when it comes time to make decisions. But is it necessary for NRC to duplicate from scratch all the industry work on generic issues before NRC can accept that work as part of an issue resolution? Specifically, what can NRC and the industry do at the front end of a research project to head off any protracted arguments over the interpretation of conflicting results at the back end?

For example, can we not do a better job of agreeing up front on what is the problem to be solved and what constitutes an acceptable technical resolution? How about common agreement on the data that are needed to resolve the problem and how the data ought to be collected? Common development of computer codes to analyze the data? Common use of experimental facilities? Now, of course, the NRC has to analyze and interpret the data independently, but can't the confirmatory research process be made more cost effective and more efficient by eliminating as many variables as we can from differing studies of the same phenomenon?

Another area of questions that you might consider has to do with the approach to research. One way of evaluating the current NRC approach to research might be to take a vertical slice, and I think certainly the NRC people in the room recognize the phrase "vertical slice" as very common these days given the current situation with concerns about licensing basis. But to take a given area of research like thermal hydraulics or seismic, in this case what vertical slice means is to pick specific research programs and work products in that area and evaluate them from the point of view of their end use. What were the work products from each one of these programs? If this was confirmatory in nature, how were these work products used in the decisionmaking process? Did the output meet the program office's stated needs, and was it used as it was envisioned in the user need letter and, if not, why not? If the work product was meant to confirm independent industry analysis, did it? Did the industry and the NRC conclusions agree? If not, why not? Was there uncertainty because of differences in the experimental setup or the way the data were collected or basic assumptions in the computer codes that were used? Could the industry program have been better leveraged to reduce the resources that NRC spent on the front end of the experiment? And, finally, how can the lessons learned from this review, and maybe periodic reviews like this, be factored into future decisions on existing programs?

Probably the final area in which it might be productive to ask questions has already been raised by the NRC as they proceed with their strategic reassessment. And it has to do with the differentiation between research with a small "r" and research with a capital "R." I'm referring to what the Office of Research does besides research.

Where is there overlap between the Office of Research activities and the program offices within NRC? For example, when it comes to initiating research or to maintaining areas of technical expertise? Should the Office of Research continue to be responsible for drafting regulations and regulatory guidance? Would it be more efficient for the program offices to do this since they are the ultimate users and implementers and will monitor how this guidance is used? Should the program offices be responsible for any confirmatory research or should it all be centralized in the Office of Research?

All these questions really fall under a broader heading of what should be the future role and scope of NRC's research program. Those words have been used to characterize what NRC calls a key direction-setting issue in the strategic assessment and rebaselining effort that is now under way. The nuclear industry agrees that this question is important enough to rise to the level of an issue that determines the NRC strategic direction for the future. The environment is changing, but research will always be essential to nuclear regulation. Research ensures that regulatory actions have a sound technical basis, are clearly understood by licensees and the public, and it provides the NRC with the tools it needs to anticipate change and to deal with emerging safety issues and advances in the state of the art of the technology.

The nuclear industry believes that NRC can maintain a viable and productive research program even with diminished resources by judiciously focusing on a smaller number of programs with the highest safety and regulatory significance, by maintaining active international collaboration, by leveraging industry programs and resources, and by addressing the types of questions we have raised today. The industry is going to do its best to respond to the strategic assessment issue paper on NRC's research that is now out for public comment, and we are ready and eager to provide constructive input as the NRC develops its strategic plan.

**24TH WRSM PANEL DISCUSSION: "THE ROLE OF RESEARCH IN NUCLEAR
REGULATION: THE CASE OF QUALIFIED IMPORTERS"**

by
Prof. A. Alonso, Counsellor
Spanish Nuclear Safety Council

Scientific knowledge and technological maturity are needed to establish regulatory requirements, what also needs talent and skills. Scientists are rarely interested in regulation and regulators not always are closely connected to scientific research. This has created gaps in regulations, mainly within qualified importers.

A qualified importer, in the sense of this presentation, is a country who has acquired nuclear power plants from more technologically advanced exporters but with an increasing participation of its own industry and institutions in the design, construction, component manufacture and assembly of such nuclear power plants and is fully responsible for the operation of the nuclear units and the corresponding fuel cycle. These countries have also a long standing and independent nuclear regulatory organization and the corresponding technical body. Spain is a qualified importer. The majority of the countries with nuclear power belong also to this kind.

It is not uncommon among the qualified importers to have a diversity of plants. For instance, Spain has W-PWR's and GE-BWR's of different vintages and families, as well as one KWU-PWR. These two facts: being a qualified importer and having a diversity of nuclear power units, shape the research the country is able to perform and the way in which the results can be incorporated into regulation.

Since the early days, it was frequent among qualified importers to use, within this priority: the codes and regulations of the country proper; those from the international organizations from which the country is a member, such as the IAEA or the UE, as in the case of Spain, and finally the codes and standards of the country of origin of the project. At least at the beginning, qualified importers have only a basic law, in most cases complemented with decrees of government, and a very limited number of safety guides. In general, limited use is recorded of the IAEA codes and safety guides, even including those in the Nuclear Safety Standard (NUSS) series. Therefore the qualified importers in the West have basically used the USA regulations, mainly those in 10CFR Part50 and others, together with US NRC's Regulatory Guides, Generic Letters and Branch Technical Positions. In Eastern Europe use was made of the scanty ex-Soviet regulations. In the case of Spain, the late introduction of the German KWU plant forced the regulatory authority to look at the German Regulations, which were also followed up to the maximum possible extent.

There are two aspects which cannot be imported: the site for the nuclear power plant and the responsibility for its safe operation. Site selection requires the knowledge of the particular site characteristics, only to be found within the country, forcing needed research and development programmes to determine soil characteristics, tectonics, seismicity, hydrology and meteorology, to name a few. Nowadays, probabilistic safety assessments are being performed, including the analysis of external events as input parameters, what is requiring an individual more in depth analysis of the seismic nature and other characteristics of the different sites.

The responsibility for safe and economical operation has also prompted research to cope with early aging problems mainly related to erosion and stress corrosion cracking of materials. In Spain, for example, stress corrosion cracking of circulation loops in the old GE-4/MARK-I BWR and later the defaults found in the core shroud and jet pump supports of such plant, prompted the establishment of a materials research programme which was conducted by the National Research Center under the auspices on the plant owner/operator.

The serious defects found in the Westinghouse model D-4 steam generators in the early 80's affected four Spanish W-3 loop units and it became a safety issue. After the basic repairs were performed, it became clear that the Inconel 600 MA used in the manufacture of the tubes was not a good selection for the environmental conditions in place and a rather substantial research programme, sponsored by the plant/owner operators, was initiated and conducted in the National Research Center.

The two examples just mentioned provided basic information from which to derive the corresponding regulations. Intents were indeed performed regarding, for instance, the maximum allowable rate and the acceleration of the release from the primary to the secondary side of steam generators. But at the end, the corresponding regulations from the US NRC were adopted as such problems were also present in the American *reference plants* and solved, in cases, in a more expeditious way.

In the early 80's the large research efforts in the area of basic thermal hydraulic transients was coming to an end in both the USA and Europe. In the USA the LOFT Programme was finished, but the facility was still useful and offered to the Nuclear Energy Agency of the OECD countries. The OECD-LOFT Programme was probably the first and most important truly international research programme ever conducted. Spain soon joined the programme and heavily participated in the evaluation of the experiments performed through a consortium in which the Regulatory Authority, the Nuclear Power Plants, the National Research Centers and some Universities participated jointly. As in the previous cases, the considerable knowledge and experience gained did not convert itself into any regulatory prescription, which were already clear in the US NRC and other regulations, but it certainly served to gain self confidence in the analysis of thermal hydraulic transients, both within the regulator and the industry, and to become more independent from the reactor supplier.

In the mid 80's severe accident research was in full swing in the most advanced countries. Spain became an early partner in the Cooperative Severe Accident Research Program under the auspices of the US NRC. It is difficult for qualified importers to build and exploit large research facilities for severe accident research; in general, those countries have not the infrastructure for that neither the needed scientific and technological skills. What such countries can do efficiently is to participate in international research programs, such as the already mentioned CSARP and the EPRI

driven LACE, ACE, MACE and ACEX which have also being open to Eastern European Countries. More recently, the French PHEBUS-FP Program is providing new opportunities for international collaboration.

The knowledge which is being gained from such activities is serving to define regulatory positions in severe accident management and other regulatory activities, which are being pursued without losing track of what is being done in the most advanced countries. For instance, the direct containment experiments performed at the Surtsey facility gave regulatory response to certain cavity designs; two Spanish cavities are somewhat different from the tested Zion and Surry, so it was decided to perform confirmatory tests for the Spanish geometries.

The funds for the research described and for the participation in the international programmes is always a problem. In the case of Spain it comes mainly from three sources. The most significant is the contribution of the electrical utilities themselves, either directly or as a consequence of a specific law. The country has a law establishing that the electrical utilities must segregate three per thousand of the total revenue from selling electricity and spent that into related research for any source of electricity. A fraction of that money, up to ten million dollars per year, has been traditionally used in research for nuclear energy and a fraction of that for nuclear safety. Apart from that, the utilities individually may invest in research for their own purpose.

The Nuclear Regulatory Council invest a few -two to three- million dollars per year in research aimed at its own regulatory purposes. The Council has a five year research program which is reviewed every year. It covers three main areas: nuclear safety, radiation health, radioecology and site related issues. The transfer of such knowledge to regulatory purposes constitutes a constant preoccupation.

The European Union, through the so called Frame Work Research Programmes on Nuclear Fission, provides an extra source of money, as it may cover up to 50% of the cost of a given research project. Apart from that, the Union is encouraging multinational and multi-institutional projects, what so creates a pool of knowledge and increases the benefits obtained in participating in such well designed research programmes.

In summary, qualified importers, like Spain, participate in research, even though it may not translate itself into regulation. Such participation will always serve to give self-confidence and independence to regulators and licensees, to better understand the adopted regulations of the most advanced countries and to solve specific problems.

THE ROLE OF RESEARCH IN NUCLEAR REGULATION: A KOREAN PERSPECTIVE

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Nuclear Safety Technology Division
Korea Institute of Nuclear Safety

I. Introduction

Good Morning ladies and gentlemen:

It is my honor to have this opportunity to take part in the panel on "The Role of Research in Nuclear Regulation". I would like to make a few short remarks on this topic from a Korean perspective.

Korea has carried out a very ambitious nuclear power program since the 1970's as part of the nation's industrialization policy. Ever since, Korea has also maintained a strong commitment to nuclear power development as an integral part of the national energy policy which aims at reducing external vulnerability and ensuring against a global fossil fuel shortage.

The introduction of nuclear power plants in Korea can be divided into three stages. The first stage (Generation I), consisting of the first 3 units which were constructed in the 1970's, progressed in the form of a turn-key based contract, because the domestic infrastructure was not developed enough to construct nuclear power plants. In this stage, all the processes, including designing, manufacturing and construction, were done by foreign suppliers. Therefore, the Korean nuclear power industries could not develop and then localize nuclear power related technologies. In the next stage (Generation II), consisting of 6 units of 900 MWe class, a utility company was in charge of the project management. Designing and manufacturing of the primary system was contracted by foreign suppliers through a so-called, component approach. Involvement of domestic industries was expanded, and self-reliance of technology was further improved through this stage. The third stage (Generation III) began with Yonggwang Units 3&4 contracted in 1987. In this project, domestic industries participated as the main contractors based on experiences gained through the previous stages.

Now, we have developed the Korean Standard Nuclear Power Plant (KSNPP) and are performing design and construction of the Ulchin Units 3&4 (first KSNPPs) project independently, under the total responsibility of Korean entities. As of October, 1996, 11 units of nuclear power plants are in operation with a total capacity of 9,616 MWe. Seven units (including 3 PHWRs) are under construction, and an additional 10 units including 4 units, of what we call, "Next Generation Reactor", which are similar to the ALWR in the U.S. in major design features, are being planned. The basis of Korean strategy for nuclear reactors is that PWRs and PHWRs will serve as the main and supplementary reactor types, respectively, for the

short and intermediate term. In the meantime, Liquid Metal Reactors may be considered as the long-term option.

According to the Korean Government Long-term Electric Power Development Program (LEPDP) established in December 1995, a total of 28 nuclear power plants will be in operation by the year 2010.

II. Nuclear Safety Research in Korea

The nuclear regulatory organizations in Korea are composed of three parts. A national level decision-making body is represented by the Korea Atomic Energy Commission (KAEC), a regulatory authority with enforcement power is represented by the Ministry of Science and Technology (MOST) of the Korean Government, and a technical expert organization, the Korea Institute of Nuclear Safety (KINS) was established to support the MOST with technical expertise in the development of nuclear regulatory policy and also in the enforcement of nuclear safety laws and regulations. The KINS, entrusted with the regulatory functions by the Government, performs (1) safety reviews and evaluations to assure the safety of nuclear installations, (2) safety inspections for nuclear installations including radioisotopes and radiation generators, (3) development of regulatory policy, and (4) research to develop regulatory requirements and safety evaluation technology.

The Korean nuclear industry realizes these days that the promotion of nuclear power would be very difficult without the assurance of reactor safety as well as public acceptance and understanding. As nuclear reactor facilities expand, regulations require, more than ever, a strong commitment to safety assurance because safety activities should also grow and expand further to meet the public demand for the safety of nuclear power plants.

In this regard, the safety regulation for nuclear power plants under construction or planning, is performed in such a manner that their safety level would be continuously upgraded by gradually incorporating advanced design features into the system with an aim to minimizing the risk of public health and safety.

In 1992, the Korean government announced a 10-year nuclear energy R & D program to achieve self-reliance in nuclear technology and international competitiveness by the early 2000's. This program includes the development of advanced technologies and establishment of a firm foundation for research on basic technologies in such fields as Next Generation Reactors, radwaste management, radioisotope utilization, NPP construction and operation, and nuclear safety. In the field of nuclear safety, KINS as a technical expert organization is in a position to simultaneously develop advanced regulatory techniques with technological advancement in the industry, so that appropriate regulatory positions can be established and timely rule-making can be provided for the industry. To this end, KINS has not only performed joint research with experts at various universities, research institutes and industries, but also participated in various

international cooperative works with foreign regulatory bodies and international organizations such as USNRC, AECB of Canada, IAEA , OECD/NEA and so on.

Since most of the Korean NPP designs are of U.S. origin and the development of the Next Generation Reactor in Korea is closely linked to the Advanced Light Water Reactor in the U.S., the NRC's regulatory research program has inevitably been used for references in Korea. In this regard, KINS has actively participated in NRC's joint research projects such as the International Piping Integrity Research Group (IPIRG) program, the Code Application and Maintenance Program (CAMP), the Cooperative Severe Accident Research Program (CSARP) and the Reactor Ageing Program. These kinds of joint research projects have been providing useful information and knowledge for the resolution of current safety as well as regulatory issues in Korea.

III. Role of Research in Korean Nuclear Regulation

In association with social achievements and advancements such as the improvement in the standard of living, democratization and local self-government operation in Korea, the level of public acceptance toward nuclear safety has grown and the anti-nuclear movement has increased significantly. Under these circumstances, the Korean Government reaffirms that nuclear safety takes the first priority in the development of nuclear energy and has continuously endeavored to improve regulatory systems and technical standards in order to enhance the safety of nuclear facilities. Safety regulations in Korea have been steadily improved through KINS' research activities. For Ulchin Units 3&4 which are under construction, for example, a number of new design features were introduced in addition to the safety depressurization system (SDS) and an alternate AC (AAC) source which were already adopted in the reference plants, YGN Units 3&4. For YGN Units 5&6, PSA is underway with an extended scope of analysis and assessment, including low power and shutdown operations, to name a few.

Major areas of research in progress at KINS are as follows:

- (a) Severe accident studies and the introduction of risk/performance based regulations.
- (b) Assessment of piping integrity and pressure vessel ageing
- (c) Development of guidelines for in-service test (IST) and evaluation guides for human factors and digital I&C.
- (d) Development of radiological safety assessment technology.
- (e) Development of safety and regulatory requirements for the Next Generation Reactors.
- (f) Improvement of the current regulatory systems and procedures for nuclear power plant

In the near future, we must also establish new regulatory procedures and requirements that will be applied to the selection and operation of radwaste disposal facilities, development of liquid metal reactors, and the use of advanced nuclear fuels. To support these activities, nuclear safety research at KINS will be conducted in such areas as the development of regulatory policy

& operating systems, development of regulatory requirements, and development of safety assessment & evaluation technology.

The role, as well as goal, of safety research at KINS can be categorized as follows:

- To build public confidence in nuclear regulation with the improvement of regulatory policies & operating systems,
- To establish, systematically, regulatory standards and guides in order to enhance the safety of nuclear facilities,
- To improve regulatory capability so that appropriate regulatory positions can be established and timely rule-making can be provided, and
- To resolve safety issues identified through the operation of nuclear power plants.

IV. Conclusion

The success of Korea's nuclear power program depends in large part on how to insure safety. Safety has the highest priority in nuclear energy development. Public acceptance has been the most critical problem faced by the nuclear industry in Korea. The public demands the highest level of safety all through the design, construction, and operation of nuclear power plants.

We have learned that the nuclear power plant designed with well addressed safety, implementation of a well established QA program during construction works and a proven record of operational safety are the only way to earn public support. We also confirm the importance of safety regulations. Competent and efficient regulation with a strong safety culture and openness in all issues is the most desirable image for regulators we strive for. Regulatory research will obviously help regulators achieve this goal of competent and efficient regulation.

Before I end my remarks, I would like to mention the importance of international cooperation in regulatory research. We know that most safety issues involved in nuclear power plant design, construction, and operation are, to a certain degree, common to all countries that have nuclear power plants. The resolution of these safety issues will obviously benefit everyone. Therefore, international cooperation in safety and regulatory research with the sharing of resources and information will pave the way to achieve this goal.

Summary of High Burnup Fuel Issues and NRC's Plan of Action

Ralph O. Meyer
Office of Nuclear Regulatory Research
U.S Nuclear Regulatory Commission

For the past two years we have concentrated mostly on the so-called reactivity-initiated accidents -- the RIAs -- in this session of the Water Reactor Safety Information Meeting, but this year we have a more varied agenda. RIAs are, of course, not the only events of interest for reactor safety that are affected by extended burnup operation. We have now had enough time to consider a range of technical issues that arise at high burnup, and a list of such issues being addressed in our research program is given here.

1. High burnup capability of the steady-state code (FRAPCON) used for licensing audit calculations
2. General capability (including high burnup) of the transient code (FRAPTRAN) used for special studies.
3. Adequacy at high burnup of fuel damage criteria used in regulation for reactivity accidents.
4. Adequacy at high burnup of models and fuel related criteria used in regulation for loss-of-coolant accidents (LOCAs).
5. Effect of high burnup on fuel system damage during normal operation, including control rod insertion problems.

We make a distinction here between technical issues, which may or may not have direct licensing impacts, and licensing issues. The RIAs became a licensing issue when the French test in CABRI showed that cladding failures could occur at fuel enthalpies much lower than a value currently used in licensing (1). Fuel assembly distortion became a licensing issue when control rod insertion was affected in some operating plants (2). In the following, these technical issues will be described and the NRC's plan of action to address them will be discussed.

FRAPCON Code

The old GAPCON code, used for years by NRC for licensing calculations, and the later FRAPCON code had been assessed in the early 1980s with data out to only about 40 GWd/t. However, a number of non-linear effects have been observed more recently at higher burnups, raising questions about extrapolation to higher burnups with the codes. We concentrated on the FRAPCON code and updated it with high burnup data for applications out to about 70

GWd/t. This work was described last year and has been largely completed (3). FRAPCON is currently undergoing a peer review, and the release of the revised code (FRAPCON3) is expected early next year.

FRAPTRAN Code

The NRC's FRAP-T transient code was last revised and documented in 1981 (4). It contains questionable failure models, which do not apply to some important situations like RIA; it has a cumbersome routine for automatic uncertainty analysis, which has seldom been used; and it does not contain high burnup effects. Work has just been initiated to take advantage of high burnup developments for FRAPCON and to make numerous general improvements to FRAP-T. The resulting modified code will be called FRAPTRAN.

RIA Criteria

This topic has been discussed extensively at the previous two Water Reactor Safety Information Meetings and will be discussed further in subsequent papers in this session (5,6). Based on test results and analyses to date, it is likely that cladding failure will not occur in modern high burnup fuel if subjected to an RIA in a power reactor. This tentative conclusion, however, is based on circumstantial evidence as none of the test programs to date has approximated power reactor conditions during RIA tests with high burnup fuel. If non-failure can be substantiated, a range of issues related to dispersal of fragmented fuel will be avoided. The NRC's research strategy is to pursue the confirmation of non-failure and to modify licensing criteria accordingly. No research on the behavior of fragmented fuel is planned at this time.

LOCA Criteria

This topic has not been discussed previously at the Water Reactor Safety Information Meeting, so the technical issue will be described here briefly. Licensing analysis for LOCAs involves embrittlement criteria from 10CFR50.46 and the use of models similar to those described in Appendix K of Part 50.

- 10CFR50.46 Cladding Embrittlement Criteria
 - 2200°F peak cladding temperature
 - 17% cladding wall thickness oxidization
- Appendix K Features of Evaluation Models
 - Baker-Just oxidation rate correlation
 - Approved model for degree of swelling
 - Approved model for incidence of rupture
 - Approved model for flow blockage

It is clear that the effects of high burnup and the improved cladding alloys, which are designed to be more resistant to oxidation, will affect all of the above. It is not clear that these changes will adversely affect safety analyses. The NRC's research strategy for this technical issue is to determine embrittlement criteria and modeling features applicable to high burnup fuel so that quantitative analyses can be done. If those analyses show that high burnup fuel is not limiting, regulatory criteria should not have to be changed. If those analyses show that high burnup fuel can be limiting, changes to the regulations would have to be considered. Embrittlement criteria and modeling features for high burnup fuel will be determined in a program at Argonne National Laboratory and supplemented by similar research in progress in France. Both of these programs will be described in subsequent papers in this session.

Normal Operation

Other fuel design limits and analyses are used in licensing to show that fuel cladding is not breached and that dimensional tolerances are maintained during normal operation. These are found in the Standard Review Plan under Fuel System Damage and include mechanical properties, cladding corrosion, irradiation growth, and internal gas pressure (7). The recent problems with control rod insertion, which will be discussed in several subsequent papers in this session, fall in this category and have been followed very closely by the NRC research staff. Because these limits and tolerances are related to normal operation, fuel vendors and licensees can address them with operational data and NRC research involvement can be minimal. Nevertheless, a review is being done for NRC by Argonne National Laboratory to comment on the likely effects of burnup on fuel damage processes under all conditions including normal operation. That review has not yet been completed and will be reported in the near future. No further research is planned by the NRC on the behavior of fuel under normal operating conditions.

Finally, to show who is doing this work for the NRC and how we are utilizing results from international programs to address our technical and licensing issues, I have listed below the various contracts, grants, and agreements that are in place during the current fiscal year, 1997.

- FRAPCON and FRAPTRAN Code Modifications at PNNL
- Cladding Metallurgy Studies at ANL
- Analysis of Reactivity Transients at BNL
- Formal Agreement with IPSN (France) on CABRI Tests and Related Work at Cadarache, Grenoble, and Saclay
- Formal Agreement with JAERI (Japan) on NSRR Tests

- Formal Agreement with RRC-KI (Russia) on IGR Tests and Related Analysis
- Participation in Halden Fuels Program
- Grant at Penn. State on Mechanisms of Failure during Reactivity Transients

More than half of these programs will be discussed in the following papers in this session.

REFERENCES

1. NRC Information Notice 94-64, "Reactivity Insertion Transient and Accident Limits for High Burnup Fuel," August 31, 1994.
2. NRC Information Notice 96-12, "Control Rod Insertion Problems," February 15, 1996.
3. D. D. Lanning, C. E. Beyer, and C. L. Painter, "New High Burnup Fuel Models for NRC's Licensing Audit Code, FRAPCON," NUREG/CP-0149, Vol. 1, March 1996, pp. 141-163.
4. L. J. Siefken et al., "FRAP-T6: A Computer Code for the Transient Analysis of Oxide Fuel Rods," NUREG/CR-2148, May 1981.
5. Session on High-Burnup Fuel Behavior, NUREG/CP-0140, Vol. 2, April 1995, pp. 313-416.
6. Session on High Burn-up Fuel Behavior, NUREG/CP-0149, Vol. 1, March 1996, pp. 33-163.
7. USNRC Standard Review Plan, "Fuel System Design," NUREG-0800, Section 4.2.

EPRI Fuel Cladding Integrity Program
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The objectives of the EPRI fuel program is to supplement the fuel vendor research to assure that utility economic and operational interests are met. To accomplish such objectives, EPRI has conducted research and development efforts to (1) reduce fuel failure rates and mitigate the impact of fuel failures on plant operation, (2) provide technology to extend burnup and reduce fuel cycle cost. The scope of R&D includes fuel and cladding. In this paper, only R&D related to cladding integrity will be covered. Specific areas aimed at improving fuel cladding integrity include:

1. Fuel Reliability Data Base
2. Operational Guidance for Defective Fuel
3. Impact of Water Chemistry on Cladding Integrity
4. Cladding Corrosion Data and Model
5. Cladding Mechanical Properties
6. Transient Fuel Cladding Response

1. Fuel Reliability Data Base

EPRI has maintained a comprehensive computerized fuel performance and fuel failure data base over the years. In addition to monitoring fuel performance trends, the database is being used as feedback for ongoing R&D program emphasis and priority setting. The data base shows that fuel duties have increased significantly: burnup extension (Figure 1), increased fuel peaking, and plant uprating. To accommodate these more demanding duties, new designs and materials have been introduced. At the same time, new water chemistry has been proposed or implemented to improve plant performance. The impact of these changes on fuel performance needs to be evaluated. Figure 2 shows fuel failure rates have been reduced for BWRs and have stayed constant for PWRs. While fuel failure rates have not increased in the more demanding operating conditions, several plant operating incidences have raised concerns for fuel performance.

2. Operational Guidance for Defective Fuel

Several recent BWR fuel failures have resulted in long axial cracks in the cladding and high-activity releases to the coolant. A comprehensive R&D program was initiated to understand the degradation phenomena and develop remedial measures. Data and insights gained from all the investigations were incorporated in a computer code - DEFECT. The code can be used by utilities to predict the propensity of fuel degradation once fuel failure occurs, and to avoid the

adverse impact of fuel degradation on plant operation. The code can also be used to evaluate the effectiveness of vendor-proposed remedies.

3. Impact of Water Chemistry on Cladding Integrity

Several water chemistry changes were proposed or implemented to optimize plant performance. Specifically for BWRs, hydrogen water chemistry and zinc injection have been implemented to reduce radiation dose and protect recirculation pipe and core internals against stress corrosion cracking. Noble metal chemical addition (NMCA) has been proposed to protect BWR core components from cracking.

For the PWRs, elevated pH/lithium has been implemented to minimize the radiation field. Zinc injection, believed to protect against stress corrosion cracking of Inconel components and reduce radiation dose, has been investigated for its impact on fuel cladding corrosion.

In each case, laboratory, test reactor, and fuel surveillance programs were conducted to evaluate the impact of such water chemistry changes on fuel cladding integrity. The results of these investigations were documented and, if necessary, incorporated in the cladding corrosion model.

One of the recent concerns related to water chemistry impact on cladding integrity is crud formation and deposition. As the thermal hydraulic duty of the fuel rods is increased, subcooled boiling occurred in many of the PWR cores. These subcooled boiling conditions have led to some performance and operation difficulties. Axial offset anomalies (AOA) and crud-related fuel failures at TMI are two recent examples of crud-related performance concerns. Conditions leading to axial offset anomalies are described in Figure 3. EPRI is currently investigating the feasibility of elevated pH and enriched boron to reduce crud formation and deposition.

4. Cladding Corrosion Data and Model

The recent trend of burnup extension with higher heat rating, longer cycle length, and residence time has resulted in more demanding duty on fuel cladding. Plant surveillance programs have shown that the cladding corrosion has become the limiting factor for further burnup extension. The recent trend of plant water chemistry changes has put further demand on the fuel cladding. To help utilities maximize fuel economy and evaluate water chemistry changes while assuring fuel reliability, EPRI has been collecting cladding corrosion data and has developed a corrosion code (PFCC)⁽¹⁾. The code considers the following parameters: heat flux, coolant temperature, neutron fluence, cladding hydrogen contents, cladding intermetallic particles, heat treatment, and coolant lithium concentration. For example, the code predicts a 15% increase in cladding corrosion when switched from coordinated chemistry condition to modified coolant chemistry. The code can be used by utilities to define cladding specification in fuel procurement contract, optimize reload

configuration to minimize cladding corrosion, and quantify the impact of water chemistry changes.

5. Cladding Mechanical Properties

EPRI has conducted programs to investigate the mechanical properties of fuel cladding and core components⁽²⁾. The dependence of total elongation and ultimate strength on hydrogen concentration are shown in Figures 4 and 5 respectively. The scatter of the data is due to several factors, including hydrogen concentration and distribution, irradiation damage, sample preparation, and measurement techniques. Since the mechanical integrity of the fuel cladding at high burnup is an important issue during both normal and transient conditions, well controlled and better characterized measurements are needed. EPRI has programs in place to obtain such data.

6. Transient Fuel Cladding Response

EPRI has, over the years, invested significant resources on developing and updating steady state and transient fuel performance codes, ESCORE and FREY. The reactivity-initiated accident (RIA) simulation experiments conducted in France and Japan have raised concerns about the ability of high burnup to withstand such hypothetical accidents. EPRI has been asked by the industry to evaluate and interpret the RIA experimental results. Our assessment concludes⁽³⁾ that existing state-of-the-art analytic models can explain the observed test rod behavior. In addition, the review and evaluation have demonstrated that experiments resulting in test rod failure cannot be used to define a failure threshold for in-reactor fuel because the failures were caused by unique, uncharacteristic factors specific to individual experiments. Furthermore, the evaluation indicates the test conditions are much more severe than those possible in LWRs. A large population of experiments that did not result in failure delineate a conservative region of success for fuel rods survival during an RIA event. Detailed three-dimensional spatial kinetics analysis indicate that the high burnup fuel is unlikely to experience enthalpy increase above the region of success (Figure 6). Thus, the search for the failure threshold, which is located much above this region is of limited or no interest. Our evaluation concludes that RIA is not a concern at high burnup and therefore is not an appropriate measure for establishing burnup limits.

Conclusion

Fuel has been subjected to more demanding environment as utilities adopted more aggressive core designs and operating strategies to improve capacity factors and fuel cycle economics. Focused and well-planned R&D is crucial to ensure improved reliability and fuel cycle economics. EPRI, the research and development organization for U.S. utilities, will also collaborate with the NRC to obtain data on safety-related transient fuel behavior at high burnup.

References

1. B. Cheng, P.M. Gilmore, and H.H. Klepfer, "PWR Zircaloy Fuel Cladding Corrosion Performance, Mechanisms and Modeling", 11th International Symposium on Zirconium in the Nuclear Industry, Garmisch, Germany, September, 1995.
2. EPRI Report TR-103302-V2, "Hotcell Examination of Extended Burnup Fuel from Calvert Cliffs-1", July 1994
3. EPRI Report TR-106387, "Evaluation of Irradiated Fuel during RIA Simulation Tests", August, 1996

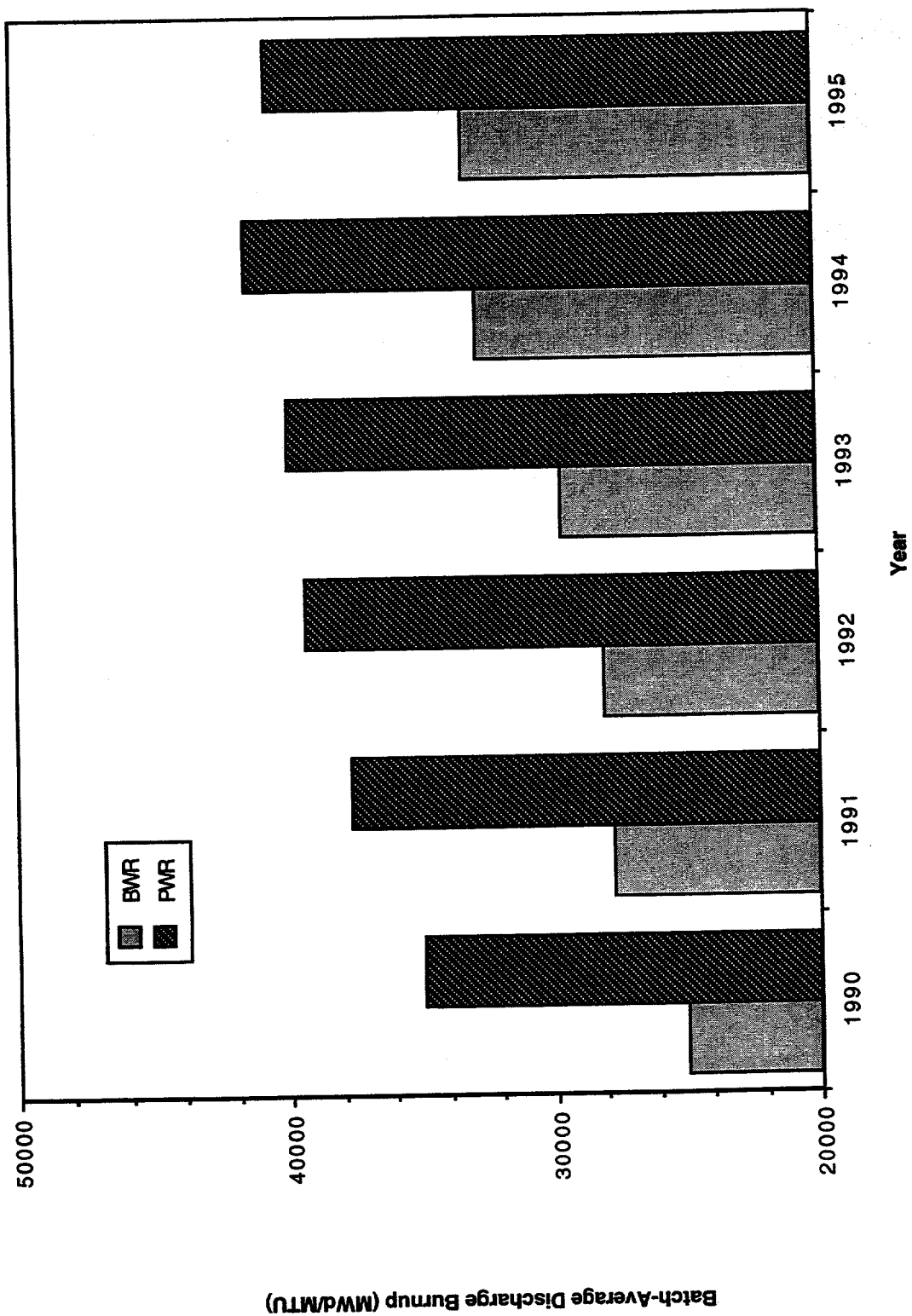


Figure 1. Trend in Batch-Average Discharge Burnups in US Reactors

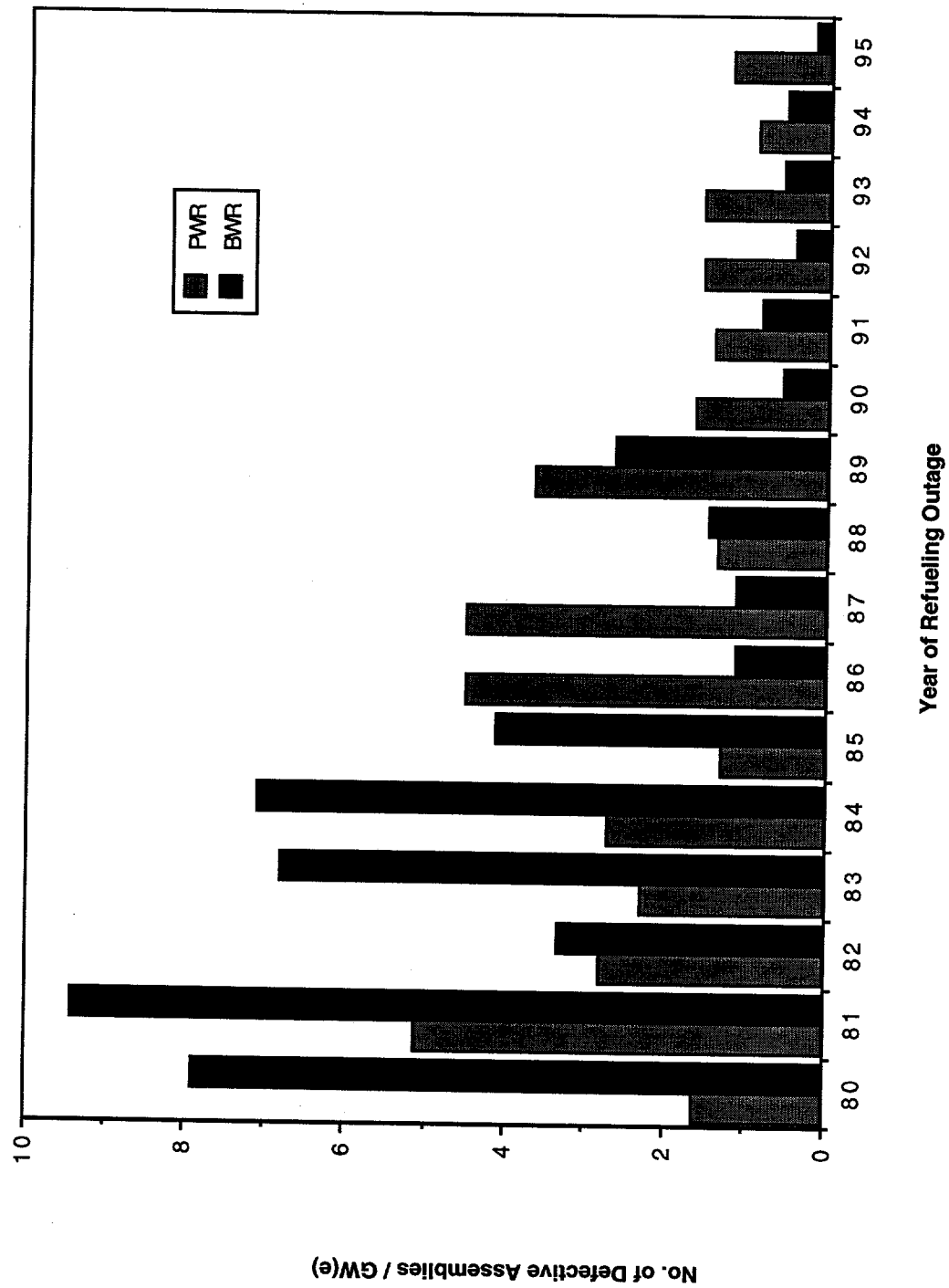


Figure 2. Fuel Failure Rates for US Reactors

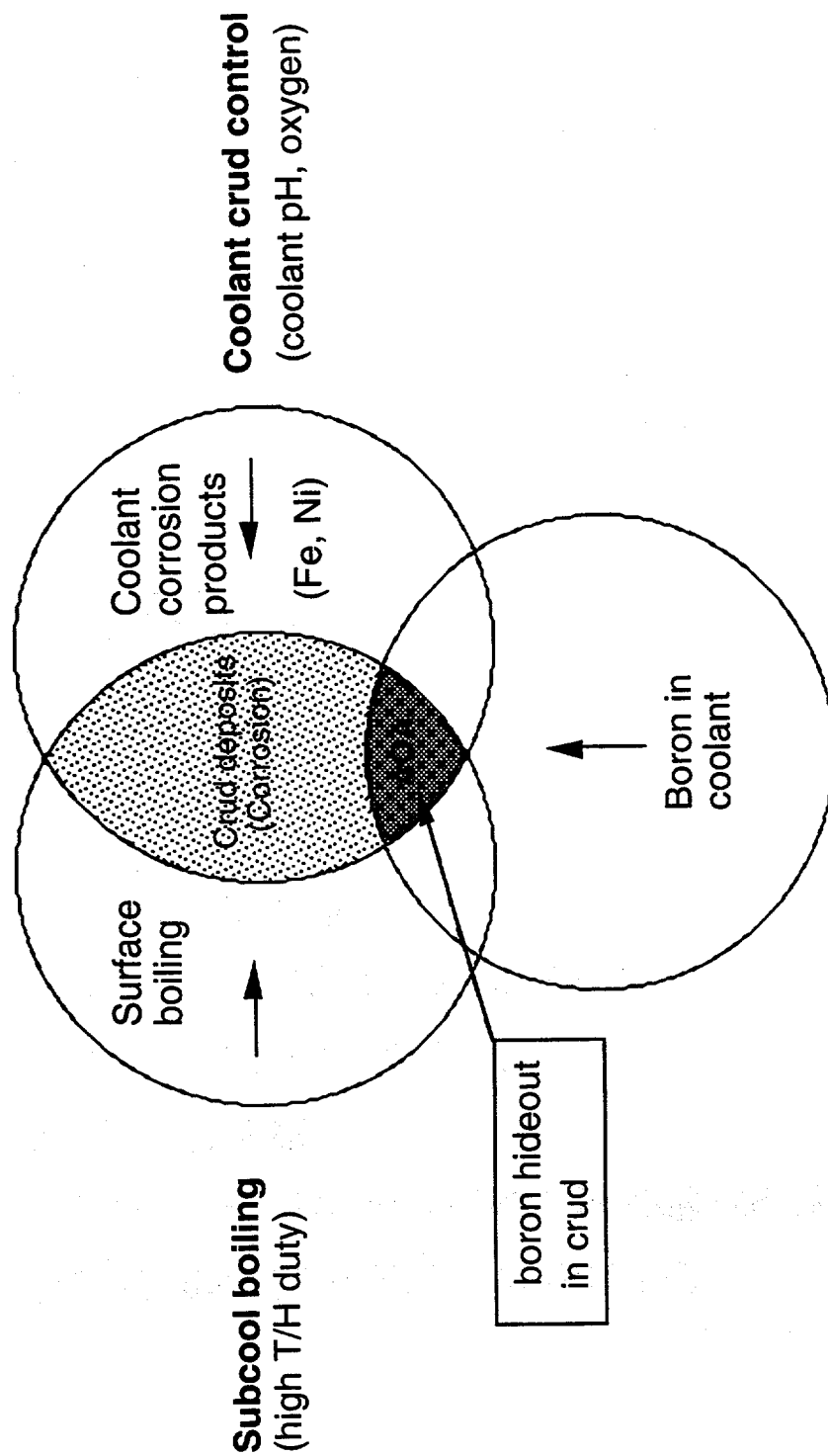


Figure 3. Conditions Leading to AOA

Calvert Cliffs and Arkansas One Unit 2

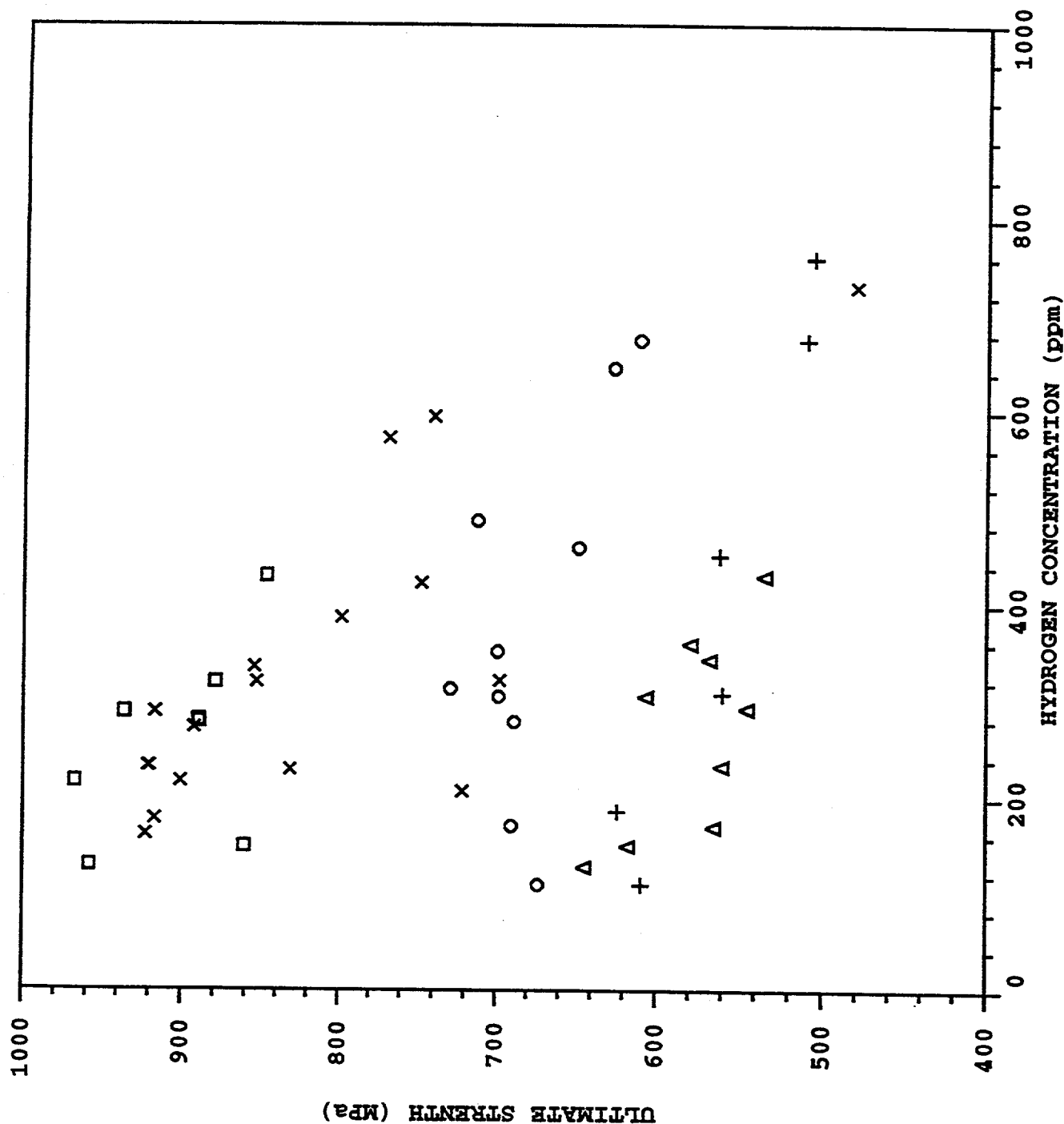


Figure 4 ULTIMATE STRENGTH - HIGH BURNUP DATA (>63 MWD/TU)

Calvert Cliffs and Arkansas One Unit 2

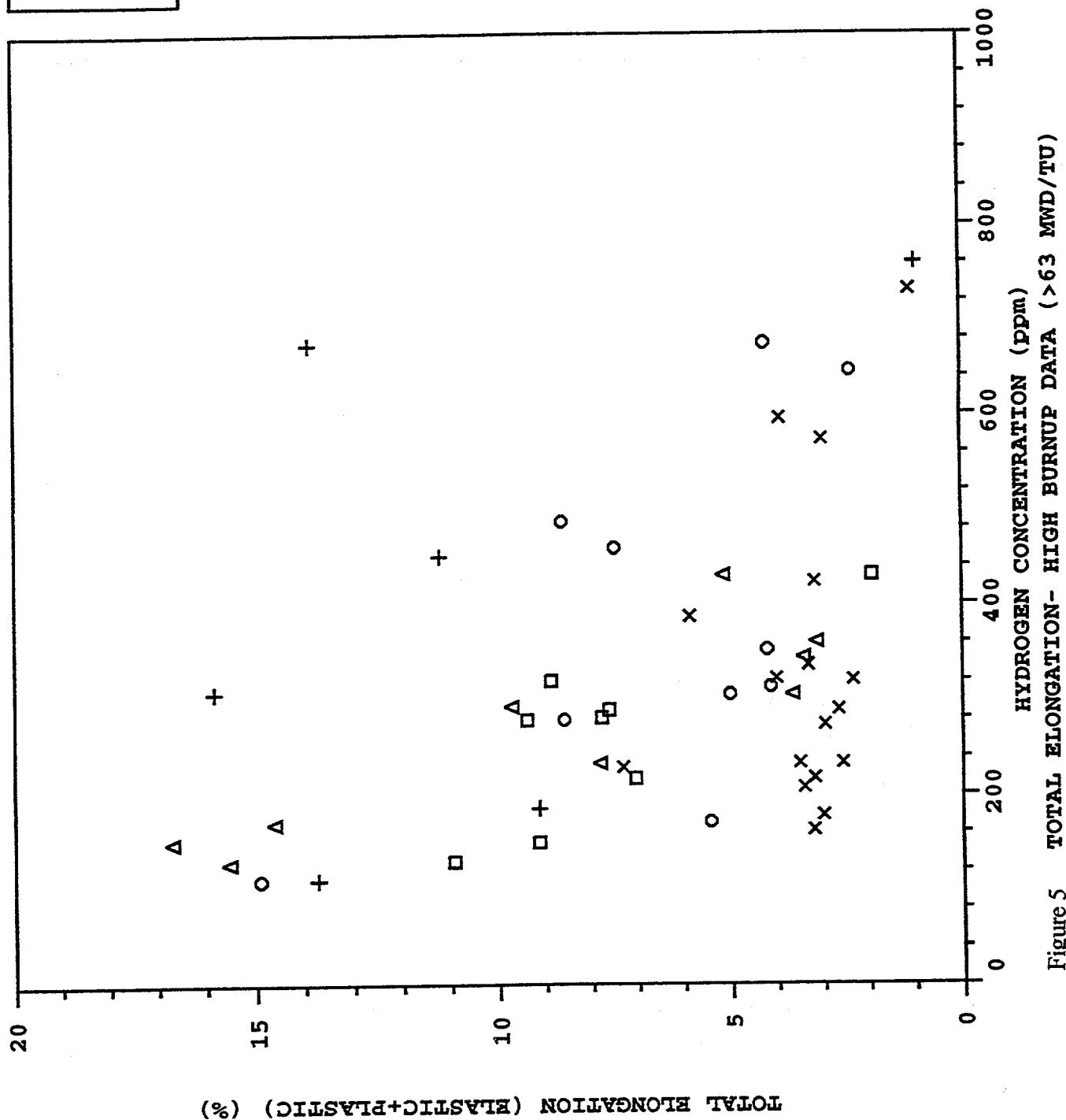


Figure 5 TOTAL ELONGATION- HIGH BURNUP DATA (>63 MWD/TU)

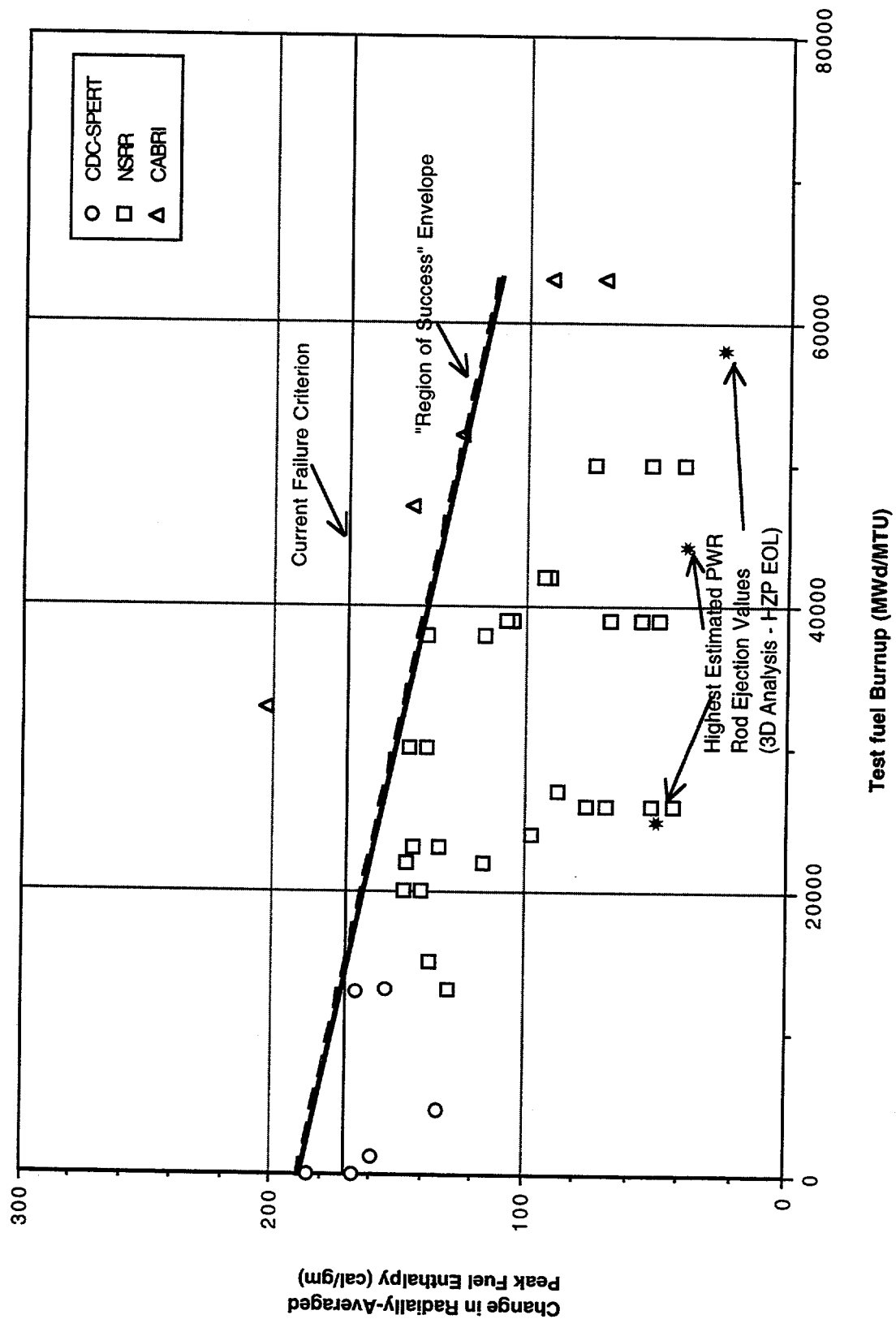


Figure 6. Description of the RIA "Region of Success"

Progress of the RIA Experiments with High Burnup Fuels
and their Evaluation in JAERI

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Recent results obtained in the NSRR power burst experiments with high burnup PWR fuel rods are described and discussed in this paper. Data concerning test condition, transient records during pulse irradiation and post irradiation examination are described. Another high burnup PWR fuel rod failed in the test HBO-5 at the slightly higher energy deposition than that in the test HBO-1. The failure mechanism of the test HBO-5 is the same as that of the test HBO-1, that is, hydride-assisted PCMI. Some influence of the thermo-couples welding on the failure behavior of the HBO-5 rod was observed.

1. Introduction

Extension of the discharge burnup of the LWR fuel assembly is the common interest in the world to improve the economy of the nuclear power generation. In Japan, the program to increase discharge burnup up to 55MWd/kgU in assembly average is progressing for both BWR and PWR plants. It is well recognized that the increase of fuel burnup brings the increase of the production of gaseous FP and their release to free gas plenum in the rod leading the increase of rod internal pressure. Increase of fuel swelling and formation of rim zone at the periphery of the fuel pellet are other concerns. For the cladding, extension of the resident time in the core brings embrittlement of the material due to increased neutron irradiation and water-side corrosion. In PWRs, reduction of the gap width between pellet and cladding become significant due to creep-down of the cladding tube under a high system pressure. These changes in the fuel characteristics will influence on the behavior of fuel rod especially under accidental conditions such as a reactivity initiated accident (RIA).

In the NSRR owned and operated by JAERI, extensive experimental study was started in 1994 with the high burnup PWR fuel and a part of the test results was reported in the 22nd and 23rd Water Reactor Safety Information Meeting. Particularly the result that a test rod sampled from the higher position of the high burnup type A PWR fuel (50MWd/kgU) failed by the hydride-assisted PCMI at the relatively low energy deposition gave us a strong impact because it agrees well with the results from the SPERT experiments and CABRI Rep

Na experiments.

In this report, new results obtained so far with high burnup type B PWR fuel irradiated in the same plant and the same period as those for the fuel used in the previous experiments (HBO-1 through 4).

2. Test Condition

Table 1 lists the design parameters of type A and type B PWR fuels irradiated in the Oi unit 2 of the Kansai Electric Power Company (KEPCO) and supplied to JAERI for the power burst experiments. The major differences between type A and type B fuels are pellet height and cladding wall thickness. Cladding material of the both fuels is zircaloy-4 of former design and not the low tin cladding. These fuels were irradiated for 4 operation cycles to attain approximately 48MWd/kgU of burnup in assembly average. Type A fuel was used in the tests HBO-1 through 4 as reported in the previous meeting(1) and type B fuel was used in the tests HBO-5 through 7 as reported in this meeting.

Table 2 gives the test conditions in the HBO test series. As shown in this table, test fuel rods for the test HBO-5, 6 and 7 were sampled from the 2nd, 4th and 3rd spans of the type B fuel, respectively and subjected to the power burst experiments. The test rod for the test HBO-5 has rather lower burnup because of the highest sampling position but higher oxidation level. The magnitude of the power burst in the tests HBO-5 through 7 is the maximum in the NSRR (pulse width at half maximum is approximately 4ms) and the same as that for the tests HBO-1 and 3. Therefore, direct comparison of the data among these tests is possible. As shown in this table, failure of the test rod was observed in the test HBO-5 and the rods used in the tests HBO-6 and 7 were survived. It should be noted that the rod for the test HBO-7 has no thermo-couple on the cladding tube.

Figure 1 shows the design of the test fuel rod used in the tests HBO-5 through 7 and is essentially the same as that used in the tests HBO-1 through 4 except slight differences coming from the differences in the design of type A and type B fuels. These fuel rods were instrumented and subjected to the pulse irradiation within the pressure-tight capsule containing stagnant cooling water at atmospheric pressure and room temperature(1).

Figure 2 shows the oxide thickness distribution in the tests fuel rods used in the HBO series. Data for the tests HBO-5 and HBO-6 were obtained after pulse irradiation. As shown in this figure, water-side corrosion in the test rods used in the HBO-5 and HBO-6 has essentially the same tendency and amount as those for the test rods used in the HBO-1 through 4.

3. Test Results

3.1 Transient behavior

Figure 3 gives the transient behavior of the test fuel rod observed in the test HBO-5. At the nearly end of the power burst, transient change in the rod internal pressure signal and a sharp pressure generation in the test capsule were observed showing the time of fuel rod failure. The cladding surface temperature at the time of rod failure was very low as in the case of HBO-1. These data suggested the failure of HBO-5 rod due to PCMI and estimated prompt energy deposition until rod failure was approximately 70cal/g.

Figure 4 compares the transient elongation of the pellet stack measured in the tests HBO-5 and HBO-6 rods which did not fail during pulse irradiation. At the time of rod failure, the pellet stack of the HBO-5 rod increased the elongation speed and attained larger elongation than that in the HBO-6 rod. This indicates clearly that the elongation of the pellet stack of the HBO-6 rod was strongly constrained due to severe PCMI. Such a constraint force in the HBO-5 rod was released due to the generation of axial crack in the cladding tube at the time of rod failure.

3.2 Appearances of the test fuel rods after pulse irradiation

Figure 5(a) and 5(b) gives the appearances of the test fuel rods after pulse irradiation in the tests HBO-5 and HBO-6. As shown in the Fig.5(a), one axial crack running all the length of active fuel region was generated in the HBO-5 rod. In case of the HBO-1 rod, similar two long axial cracks (separated 180 degree) were generated in the 90 degree position from the thermo-couple welding(1). The important finding in the test HBO-5 is that the crack run along the side where the three thermo-couples were spot-welded to measure cladding surface temperature evolution during transient. As shown in Fig.5(b), the rod used in the test HBO-6 did not show any significant damage due to the pulse irradiation.

3.3 Metallographies

Figure 6 shows the sampling positions for the metallography in the rod used in the test HBO-5. It is clear that the axial crack run near the thermo-couple welding point. Another feature observed in this photograph is many local spalling of the oxide film formed on the cladding surface. It should be noted that some degree of local spalling of the oxide film was already observed before the pulse irradiation.

Figure 7 shows the magnified view of the wall-through cracking near the thermo-couple attachment. Zone affected by the thermo-couple welding and thinning of the cladding wall there are clearly shown. In the place away from the thermo-couple attachment, typical

brittle and ductile fracture of the cladding tube was observed as shown in Fig.8 and its appearance is very similar to that observed in the test HBO-1 (1). Figure 9 shows the generation of parallel cracks where one of the cracks penetrates cladding wall and the other stopped in the middle of wall thickness.

Figure 10 shows the defects of the cladding tube in the rod used in the test HBO-6. Those are separation of heat-affected zone with many micro cracks and brittle cracks in the hydrided zone.

From these findings, it may be possible to draw a tentative conclusion that the failure mechanism of the HBO-5 rod is hydride-assisted PCMI with some influence of thermo-couple attachment and the failure threshold might be slightly affected by the spot-welding of the thermo-couples.

3.4 Fission gas release

Figure 11 shows the transient fission gas releases measured in the irradiated PWR fuel experiments as a function of peak fuel enthalpy. Figure 12 gives the same data as a function of fuel burnup. Fission gas release in the HBO-6 rod denoted by solid square was less than 10% and approximately half of the release observed in the high burnup type A fuel experiments (HBO-2 through 4). The physical reason of this phenomenon should be studied in the future.

3.5 Deformation of the cladding tube

Figure 13 shows axial distributions of the residual deformation of the test fuel rod measured after the tests HBO-3 and HBO-6. As shown in this figure, deformation behaviors of the test rods used in the tests HBO-3 and HBO-6 are very similar in spite of the large difference in the fission gas release behavior. This is another important item to be studied in detail.

3.6 Failure thresholds

Figure 14 gives the summary of the high burnup fuel experiments at this moment. We added one failure case (HBO-5) and two no failure cases (HBO-6 and 7). These data increased the importantness of the hydride-assisted PCMI to account for the behavior of fuels rods under RIA conditions.

4. Summary

The test HBO-5 with a type B high burnup PWR fuel resulted in fuel failure at the energy deposition of approximately 70cal/g which is slightly higher than that of HBO-1 rod. The failure mechanism is the hydride-assisted PCMI with some possible influence of the thermo-couple attachment. Two no failure cases at the same power burst condition were obtained by using type B test fuel rods sampled from the lower position. Fission gas release measured in the HBO-6 was relatively small (approximately half of the type A fuel rod).

Further investigation on the behavior of high burnup fuels under RIA conditions will be continued by using typical high burnup BWR fuels and PWR fuels with low tin cladding.

Acknowledgements

The tests HBO-5 through 7 have been performed as a collaboration program between JAERI and Nuclear Fuel Industries (NFI) by using fuel rods transferred from KEPCO. The authors would like to acknowledge and express their appreciation for the strong supports and efforts devoted by numerous engineers and technicians in JAERI for this important program.

References

- (1) New Results from the NSRR Experiments with High Burnup Fuel
T. Fuketa et al., 23rd Water Reactor Safety Information Meeting,
Bethesda, U.S.A., October 23, 1995

Table 1 Comparison of design parameters between Type A and Type B fuels used in the NSRR experiment

Fuel design	A type	B type
Manufacturer	Mitsubishi	NFI
Fuel pellet		
Diameter / mm	8.19	8.05
Height / mm	13.5	9
Shape	Dished	Dished, Chamfered
Cladding		
Diameter / mm	9.5	9.5
Wall thickness / mm	0.57	0.64
Experiments	HBO-1,2,3,4	HBO-5,6,7

Table 2 Test conditions in HBO series

	HBO-1	HBO-2	HBO-3	HBO-4	HBO-5	HBO-6	HBO-7
Fuel Type	17×17A	17×17A	17×17A	17×17A	17×17B	17×17B	17×17B
Sampling Position	3rd Span	4th Span	5th Span	6th Span	2nd Span	4th Span	3rd Span
Burnup / MWd/kgU	50.4	50.4	50.4	50.4	(44)	(49)	(49)
Pressurized / No-pressurized	No- pressurized	Pressurized	No- pressurized	No- pressurized	No- pressurized	No- pressurized	No- pressurized
Reactivity Insertion	\$4.6	\$3.0	\$4.6	\$3.6	\$4.6	\$4.6	\$4.6
Failure / No failure	Failure	No failure	No failure	No failure	Failure	No failure	No failure
Peak fuel enthalpy, Energy deposition by the failure / J/g·fuel	~60	37	74	50	(~70)	(80)	(80)

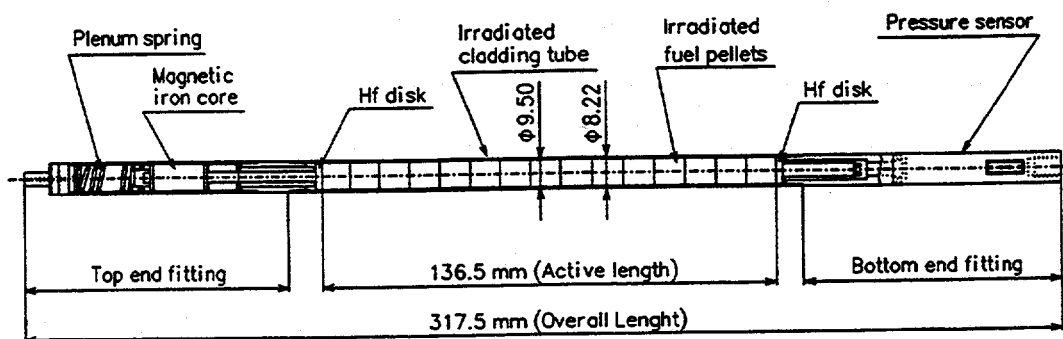


Fig. 1 Design of test fuel rods used in HBO-5 and 6 tests

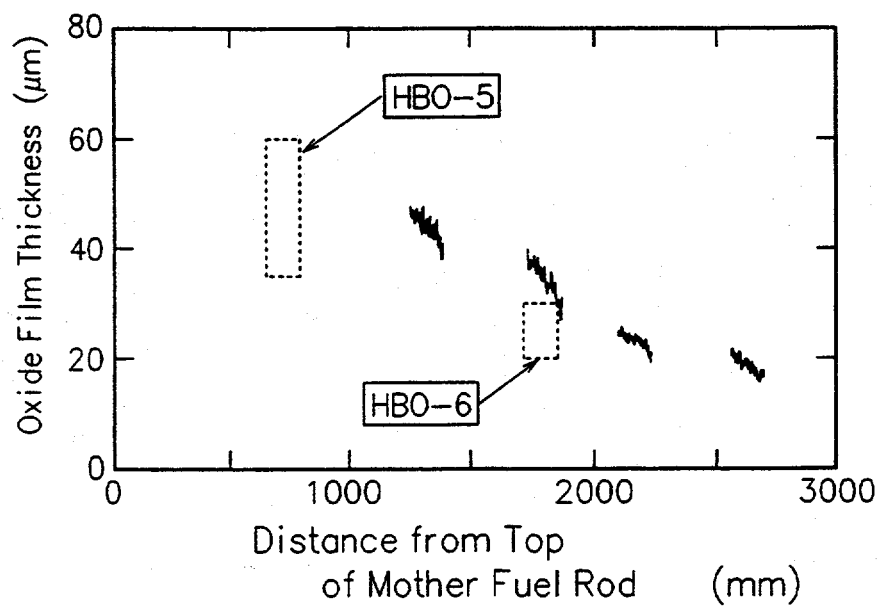


Fig. 2 Oxide thickness in the test fuel rods used in the HBO-5 and 6 tests

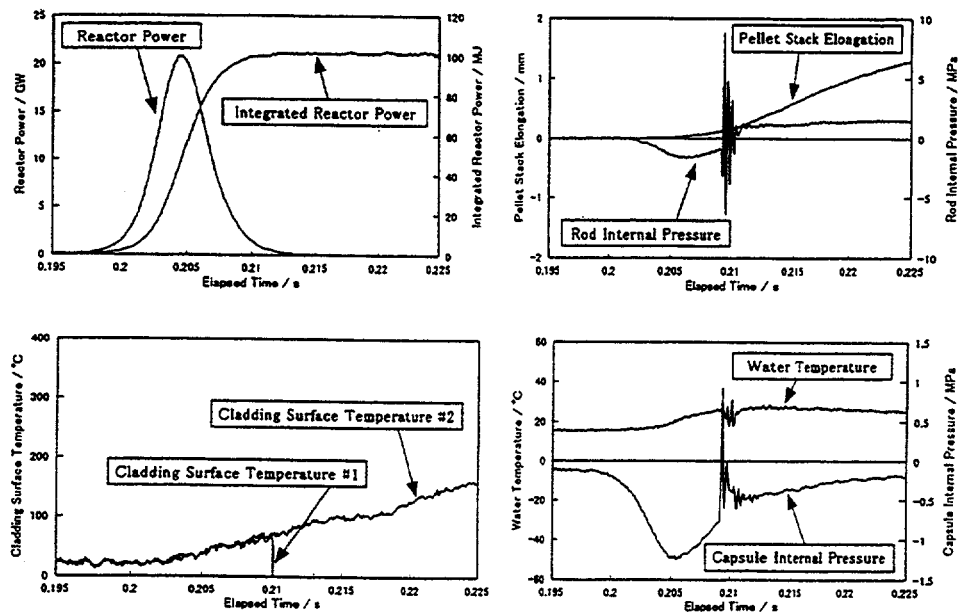


Fig. 3 Transient records in HBO-5

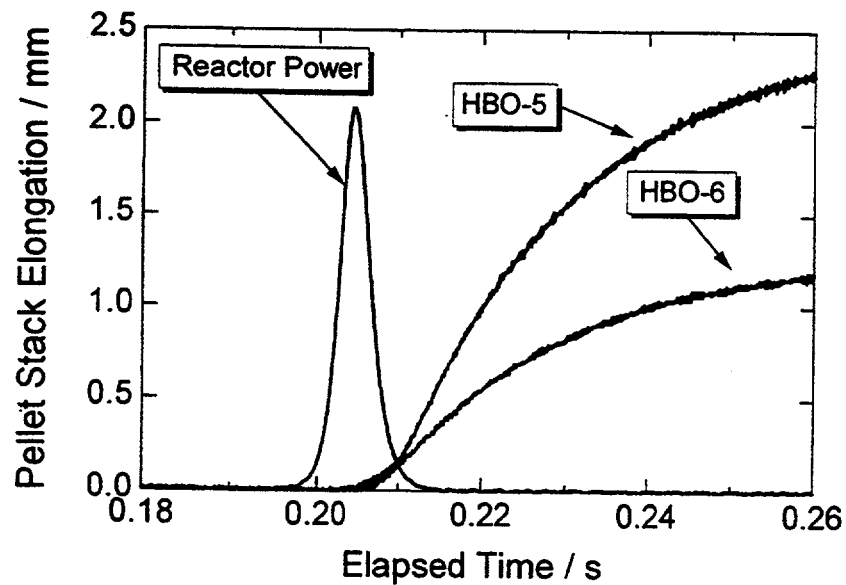


Fig. 4 Transient elongation of fuel stack measured in HBO-5 and 6 tests



Fig. 5(a) Appearance of the test fuel rod in HBO-5 test

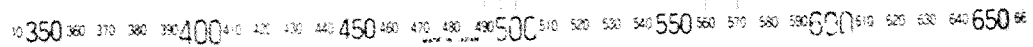


Fig. 5(b) Appearance of the test fuel rod in HBO-6 test

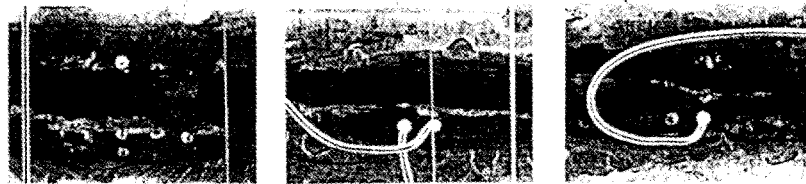


Fig. 6 Sampling positions for metallography in the fuel rod used in the HBO-5



Fig. 7 Influence of T/C attachment on the failure of the HBO-5 rod

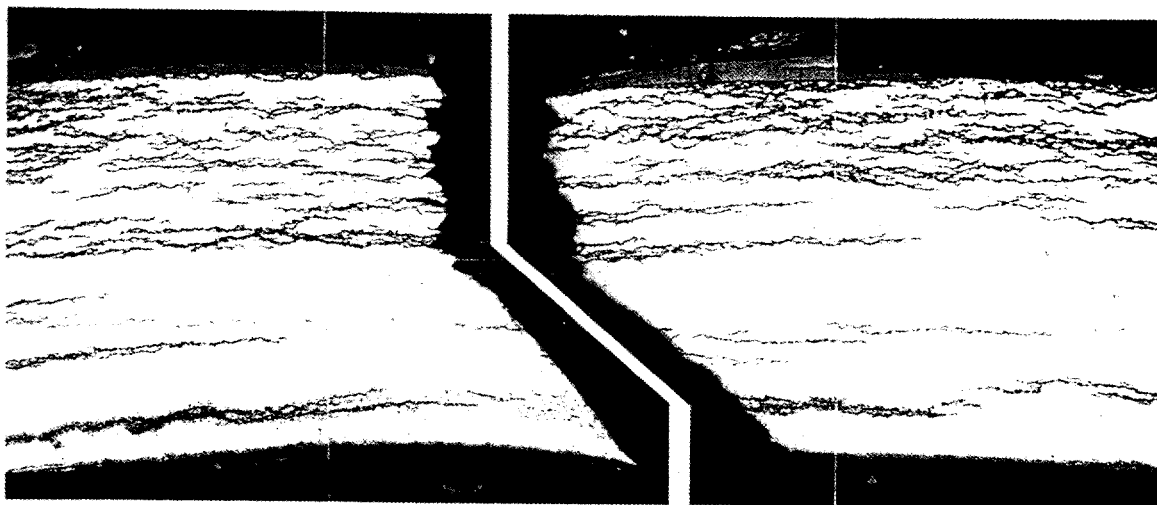


Fig. 8 Brittle and ductile fracture of the cladding tube observed in the HBO-5 rod

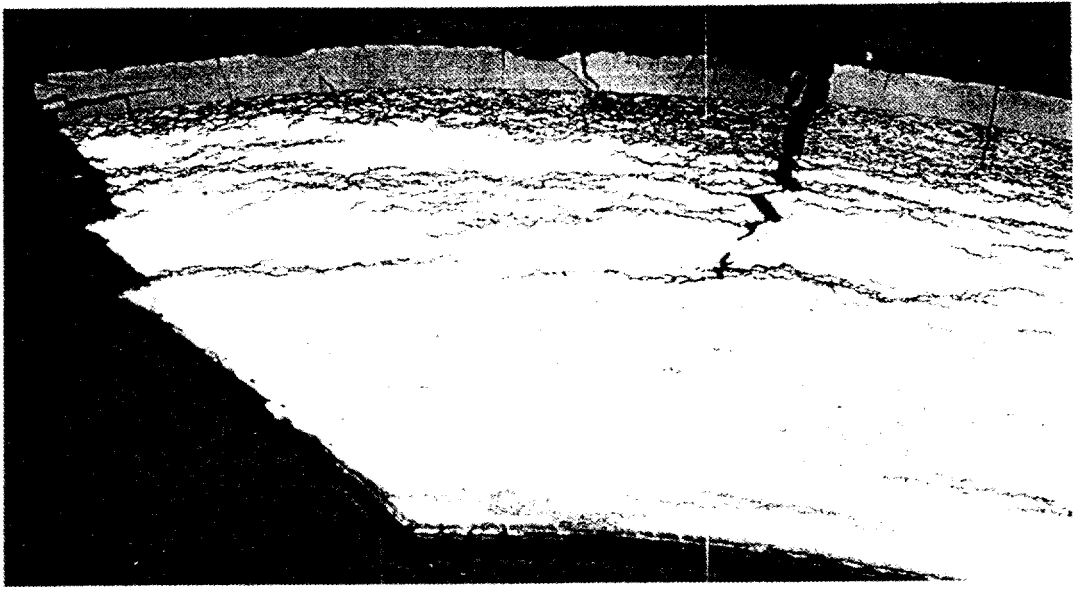


Fig. 9 Non-penetrated cladding crack generation in the HBO-5 rod



Fig. 10 Defects of the cladding tube observed in the HBO-6 rod

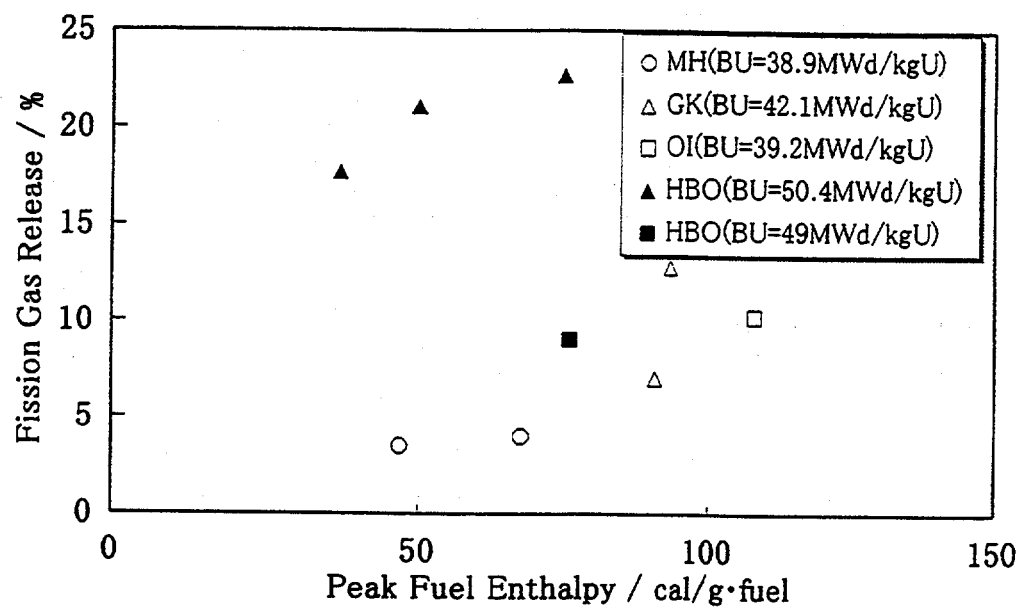


Fig. 11 FGR as a function of peak fuel enthalpy

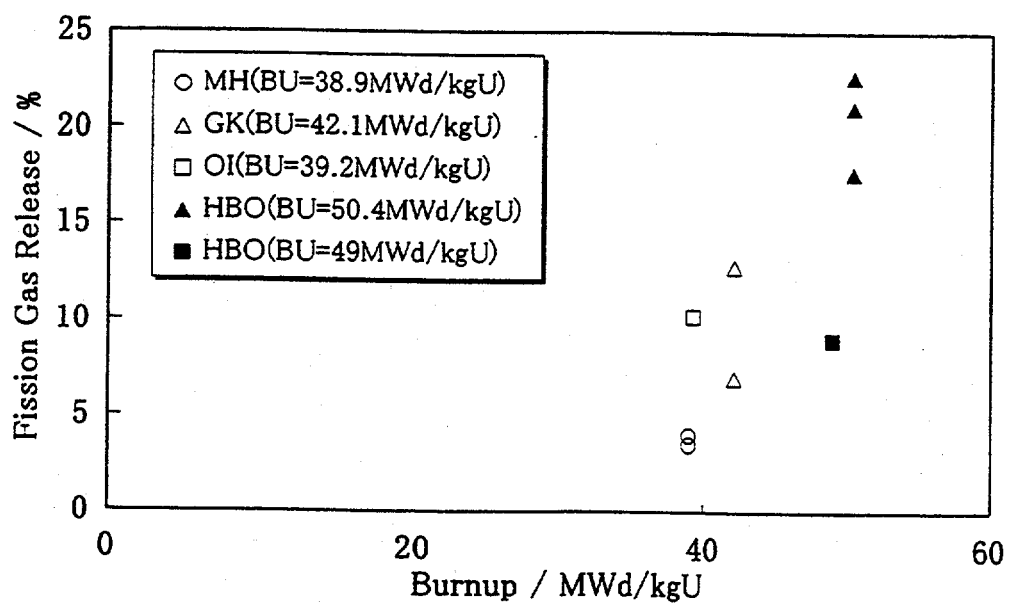


Fig. 12 FGR as a function of fuel burnup

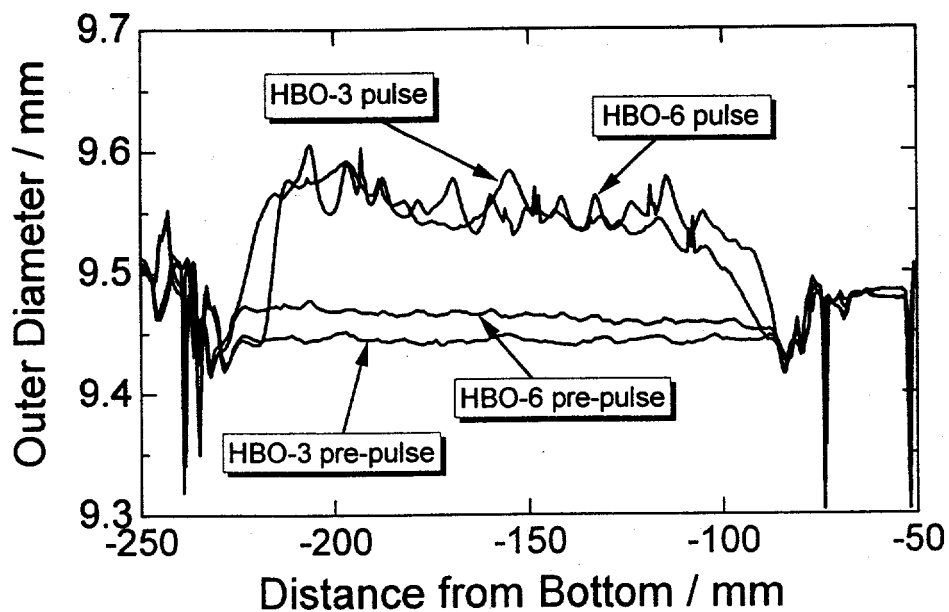


Fig. 13 Residual deformation of the fuel rods observed in the HBO-3 and HBO-6 rods

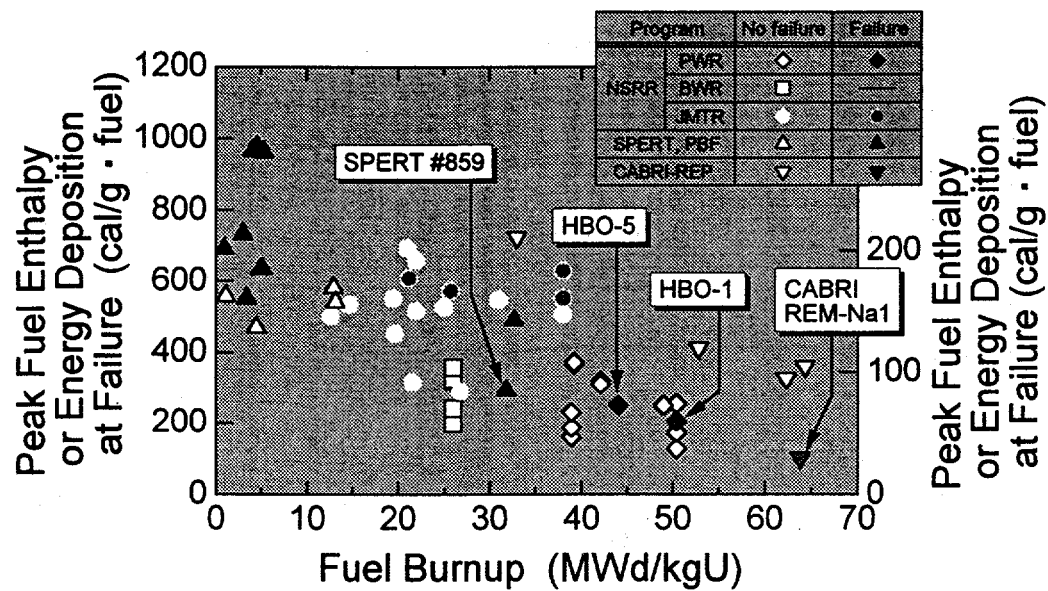


Fig. 14 Summary of the high burnup fuel tests

4. The fourth part of the document discusses the implications of the study for practice and policy. It suggests that the findings can be used to inform decision-making and to develop strategies to address the issues identified in the research.

5. The fifth part of the document concludes the study and provides a summary of the key findings. It also acknowledges the limitations of the study and suggests areas for future research.

**The status of the CABRI Test Program
Recent Results and Future Activities**

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Abstract

The first five CABRI experiments of the REP-Na series, all with UO₂ fuel and up to a maximum local burnup of 64 GWd/t, have been examined and analysed and are now reasonably well understood.

In March 1996, the first MOX test with a 3 cycle irradiated fuel at 47 GWd/t radially averaged, local maximum burnup has been successfully performed. The rod did not fail and detailed examinations are being obtained and still in progress presently. The available results and findings are presented in this paper.

Three experiments of the REP-Na test matrix are still to be performed, REP-Na7, a 4 cycle MOX test, is scheduled in November 1996. The last two experiments, REP-Na 8 the key experiment of the UO₂ matrix, and REP-Na 9, a 2 cycle MOX fuel test, will be performed during the first half of 1997.

The CABRI tests made with sodium cooling have a good representativity of reactor conditions during some tens of milliseconds. For better simulation on a longer time range, a project study has been undertaken in view of the implementation of a pressurised-water loop into the CABRI reactor. The design of this loop and the performance parameters of the upgraded driver core of CABRI is presented.

Finally, the planning of the CABRI transformation and the outlines of the future test matrix is given. The most optimistic estimation allows to predict that the first tests under prototypical test conditions could be performed before the end of 1999.

1. Introduction

Up to the end of the 1980s the cores of the Light Water Reactors PWR and BWR appeared to be well protected against the potential consequences resulting from the design basis reactivity accidents (RIAs). The knowledge of the fuel state at burnups beyond 50 GWd/t and recent results from RIA experiments with high-burnup fuel [1-6] seem to indicate that the safety criteria which have been formulated on basis of fresh or low-burnup-fuel experiments may not be applicable to high burnup fuel rods. First alarming results were obtained in the NSRR experiments JM4 and JM5 with failures well below the regulatory level and with clear indications for a PCMI failure mode, characteristic for high-burnup fuel and not observed in the earlier fresh and low burnup tests. These results have been confirmed in the tests with industrial fuel, REP-Na 1 in CABRI and HBO-1 and HBO-5 in NSRR. In all these tests a direct correlation with high-burnup phenomena could be established and in addition, the

previously not well understood test PBF-CDC 859 could be integrated into this family of failure tests.

The internationally agreed failure mode is the hydride assisted PCMI failure. Hydride accumulations occur either as local blisters or as azimuthally regular high radial concentrations on the outer rim of the cladding. They might be accidental and untypical for real fuel as in JM4, JM5 and CDC 859 or "natural" as in HBO-1, HBO-5 and REP-Na 1.

Other CABRI experiments at lower corrosion state, in particular REP-Na 4, 5 and 6 (see table 1) and also HBO-3 and 6 demonstrate a remarkable mechanical resistance against the severe loading resulting from the rapid power excursions.

Table 1 - CABRI REP-Na test characteristics and major results

Test	Test-Rod	Pulse (ms)	Energy at End of Pulse (cal/g)	Corrosion (μ)	RIM (μ)	Results and Remarks
Na-1 (11/93)	Grav5c span 5 4.5 % U 64 GWd/t	9.5	110 (at 0.4 s)	80 important initial spalling	200	- brittle failure at 30 cal/g - hydride accumulations - fuel dispersion : 6 g including particles other than RIM - pressure peaks in sodium
Na-2 (6/94)	BR3 6.85 % U 33 GWd/t	9.5	211 (at 0.4 s)	4	-	- no rupture - $\Delta\phi/\phi(\text{max})$: 3.5 % mean value - FGR/5.5 %
Na-3 (10/94)	EDF 4.5 % U 53 GWd/t	9.5	120 (at 0.4 s)	40	100	- no rupture - $\Delta\phi/\phi(\text{max})$: 2 % max - FGR/13.4 %
Na-4 (7/95)	Grav5c span 5 4.5 % U 62 GWd/t	# 60	95 (at 1.2 s)	80 no initial spalling	200	- no rupture - transient spalling - $\Delta\phi/\phi(\text{max})$: 0.4 % mean value - FGR/8.3 %
Na-5 (5/95)	Grav5c span 2 4.5 % U 64 GWd/t	9.5	105 (at 0.4 s)	20	200	- no rupture - $\Delta\phi/\phi(\text{max})$: 1 % max - FGR/15.1 %
Na-6 (3/96)	MOX, 3c span 5 47 GWd/t	35	126 at .66 s 160 at 1.2 s	40	-	- no rupture - $\Delta\phi/\phi(\text{max})$: 3.2 % max - FGR/22 %

Remaining REP-Na tests to be performed

Na-7 (10/96)	MOX, 4c span 5 56 GWd/t	35	ca 130	-	-	to be performed
Na-8 (06/97)	Grav 5c span 5 4.5 at % > 60 GWd/t	60	ca 100	-	-	cladding presenting spalling to be performed
Na-9 (06/97)	MOX, 2c span 5	35	ca 150	-	-	to be performed

All the CABRI or NSRR experiments however suffer from some unrepresentativity in the experimental conditions. In the case of CABRI sodium cooling instead of water do not allow a direct transposition to the reactor case, after some tens of milliseconds.

The performance of computer simulations with the aim to realize this transposition is limited due to the insufficient knowledge of behaviour models and material properties.

It is to be concluded therefore that representative in pile experiments are needed in order to investigate the behaviour of high-burnup fuel submitted to fast power transients, to determine precisely the safety margins and to evaluate the potential consequences resulting from post failure events.

2. Present state of Knowledge

The original data base of RIA test results has been enriched by the recent results from NSRR and CABRI. All the available relevant data are plotted in the well known diagram of the maximum mean enthalpy reached before failure as a function of the burnup of the tested fuel rod (Fig. 1).

This diagram has been used in the past for delimiting the safe area from the failure region and to define safety criteria which represented guidelines for the definition of control rod worth and for rod design.

At high burnup this procedure is not longer valid due to the irradiation induced changes which are not sufficiently defined by the burnup only and which can be different over a wide range depending on material choices and irradiation conditions :

- gap closure,
- clad corrosion phenomena (hydrogen pickup and oxide spalling),
- fission gas release and retention,
- plutonium build up and redistribution (RIM formation).

These transformations of the fuel produce in addition a strong sensitivity to the parameter of the power rise rate, determined by the pulse width of the reactivity excursion.

This parameter is changing over a wide range for the various experiments performed in different test facilities. Therefore the diagram presented in fig. 1 has to be considered with critical attention and a large uncertainty range must be envisaged for the enthalpy to failure at a given burnup due to different test fuel and incomparable test conditions.

2.1 Fuel behaviour

Fast transient heating of high burnup fuel produces new and significantly different effects compared to fresh or low burnup fuel.

The closure of the initial fuel/clad gap, resulting from the creep-down of the clad material under the effect of the PWR system pressure, leads to the immediate built up of high contact pressures when the fuel temperature rises rapidly. In addition to the thermal expansion, transient fuel swelling occurs when retained fission gas rises to high pressures inside the bubbles and porosities. Grain boundary gas produces fuel fragmentation. The working gas increases finally the internal pin pressure when it is released into the free volume of the fuel rod.

Beyond 45 GWd/t burnup, a specific structure is built up progressively at the outer rim of the fuel. Neutron resonance capture phenomena of U-238 are the reason for the locally high plutonium production which increases the fission rate and leads to locally high burnup and

associated high fission product concentrations. In this region a large temperature peaking can occur when the transient heating is close to adiabatic due to a very narrow power pulse. When the pulse width increases, the thermal disequilibrium disappears as a result from heat conduction and because the RIM is very narrow (ca 100 microns). The clad temperature and the system pressure have a strong influence on these high burnup phenomena which determine the global response of the fuel rod to the accident conditions.

2.2 Cladding behaviour

High burnup PWR cladding is characterized by severe and typical corrosion phenomena. Waterside corrosion produces the deposition of a steadily increasing oxide-layer on the cladding surface and simultaneously hydrogen dissolution in the metallic part with a steep concentration gradient over the clad thickness. These corrosion effects lead to a brittle mechanical behaviour of the cladding material and, in detail, to the shift of the brittle to ductile transition to higher temperatures.

Furthermore it is observed that increasing oxide thickness at around 80 microns or more may lead to the phenomenon of spalling e.g. local scaling-away of the oxide layer. Under nominal operation conditions spalling produces cold spots and, when this fuel undergoes a still prolonged operation, hydrogen accumulates in the cold region and hydride blisters are formed.

The mechanism of spalling is not understood in detail, load-follow operation might stimulate this evolution.

The present state of knowledge leads to postulate that at high burnup an unknown population of fuel rods of the core presents more or less advanced spalling and eventually associated hydride blisters (sun-burst).

Under the rapid transient loading of the RIA scenario the blisters act as crack initiators as observed in CDC 859, JM4, JM5 and REP-Na 1.

In the tests HBO-1 and HBO-5 no initial spalling was observed and the clad failure is to be attributed to a steep hydrogen gradient in the clad wall and to the general embrittlement of the cladding which in addition is exacerbated because of the untypical low clad temperatures in NSRR.

Furthermore the narrow power-pulse of NSRR (FWHM ca 5 ms) might be penalizing and unrealistically conservative compared to the broader pulse (FWHM ca 35 ms) which would occur in a reactor accident. This hypothesis however has no experimental confirmation at present time.

As a conclusion it is to be stated that the major risk for the occurrence of low enthalpy failures is related to the hydrogen embrittlement of the cladding and in particular to the presence of hydride accumulations.

Here again more realistic experimental conditions close to the PWR conditions might change significantly the experimental results which are presently obtained under untypical conditions.

2.3 Transient thermohydraulics

The evolution of the cladding temperature history following a given, fast RIA transient, is essentially determined by the clad-to-coolant heat-transfer behaviour. This behaviour is depending on the nature, the pressure and the flow of the coolant and sensitive to the channel geometry. Furthermore it might be significantly influenced by the state of the cladding surface during the transient sequence.

Transient spalling phenomena have been observed in several CABRI tests ; they would influence most probably the heat exchange close to the critical heat flux.

Neither in CABRI nor in NSRR the thermalhydraulics can simulate correctly the reactor situation presently.

In CABRI, only during the very short, close to adiabatic time period at the beginning of the transient, the clad temperature is correct. If a clad failure occurs in this short time interval, the result is rather pertinent even if eventual post-failure events can not be turned to account. This was the case for REP-Na 1. The absence of failure in all other CABRI tests is not conclusive, because overcooling under sodium after the first adiabatic PCMI loading phase and the impossibility to reach DNB, prevents the clad temperature from increasing like in PWR conditions. The survival of the test pins in some of the tests, in particular REP-Na 4 might be explained by the high resistance of the clad due to its too low temperature.

Representative global experiments under PWR test conditions are urgently needed.

Analytical experiments are performed in the PATRICIA loop in France with the aim to determine the transient heat transfer correlation. It is expected that the results of these tests will give a better physical understanding of the heat exchange during rapidly changing high heat fluxes.

The influence of the clad surface, typical for high burnup fuel, originally one of the test goals, cannot be simulated in these tests.

3. Recent results

The latest results, not yet reported, from the REP-Na test program concern :

REP-Na 1	Cladding fracture analysis by Scanning Electron-Microscopy (SEM)
REP-Na 4	Non-destructive and destructive post-test examinations
REP-Na 5	Profilometry, Macro- and Micro-Metallography
REP-Na 6	

3.1 New results from REP-Na 1

SEM examinations have been performed on a fractured piece of cladding sampled close to level 90 mm BFC.

The aspect of the fracture surface clearly shows three distinct zones, a first one close to the inner bord, a large intermediate zone and finally the outer zone which is larger than the inner

one (see fig. 2 and 3). Presently, the expert opinions are not really converging to a clear diagnostic. No obvious hint is given for the indication of the sense of the rupture propagation.

A first degree observation without complex interpretation, leads the author to conclude that three brittle areas of different nature are to be seen and details seem to indicate that platelets of hydrides could be at the origin of cleavage phenomena which might be more or less co-planar. The overall behaviour therefore appears to be brittle.

3.2 REP-Na 4 and REP-Na 5 results

It is of interest to observe comparatively documents from both experiments. In fact, the tested fuel rod segments were cut from the same initial reactor fuel rod at the level of span5 and span2 respectively.

The observed differences (fig. 4 to 7) are to be explained by the different degrees of corrosion and/or by the different width of the power pulse during the accident simulation.

Fig. 4 and 5 show macrographs after chemical etching. The narrow pulse of REP-Na 5 produces higher thermal loading in the outer part of the fuel.

Fig. 6 and 7 show different hydride structures resulting from the different corrosion levels.

Fig. 8 finally shows the spalling of the oxide layer which was produced during the transient. This high degree of spalling occurred despite the very moderate plastic deformation of 0.4 % in this test.

The general conclusions of these observations have already been reported in the previous paragraphs of this paper.

3.3 REP-Na 6 results

For this latest performed experiment, the first MOX-fuel test in CABRI, the examination work is still in progress.

Important straining (3.2 %) and fission gas release are observed. The transient spalling is significant but less extended than in REP-Na 4, indicating that the initial oxide layer thickness is the important parameter for this phenomenon.

Fig. 9 and 10 show the axial distribution of straining and oxide layer thickness. An important ovalization is to be stated and the spalling is directly correlated to the smaller diameter. This observation is unrefutable and it is certainly worthwhile to reach a mechanistic understanding of the underlying mechanism.

Also the origin of the ovalization is not understood presently. Actions have been initiated in order to identify the origin of this important result.

Fig. 11 to 15 show macrographs and micrographs which show that the overall response of the MOX fuel to the severe power pulse appears very satisfactory. The plutonium bearing clusters seem not affected by the transient and exhibit the morphology which is known to occur under base irradiation : RIM-like structure at the outer edge of the fuel and densified with large porosity in the center of the pellet.

4. The CABRI water-loop project

A detailed project study of a water-loop in CABRI has been performed and documented for the purpose to provide to the deciders all the necessary information about :

- the loop design,
- performances,
- cost and delays.

This new equipment inside the CABRI reactor will allow to expose representative sections of PWR (or BWR) fuel rods to the conditions which characterise the nominal plant environment :

- 155 bars (max),
- 350°C (max),
- 5 m/s (max coolant water flow).

In Fig. 16 a schematic view of the loop and its confinement is presented. Fig. 17 and 18 show details of the primary and the secondary parts of the loop. Principle discussions have been held with the safety authority and the feasibility and the geometrical implementability is fully demonstrated.

This new loop will be interchangeable with the existing sodium loop. It will take two to three months to change the CABRI configuration between two test campaigns under changing coolant conditions.

The manufacturing of the loop will take 1.5 year and the installation into CABRI will take another year. Partial overlapping of these activities will allow to reduce the full duration until availability of the facility to slightly more than two years.

It might be of interest to know that minor modifications of the proposed configuration would allow to perform tests under vapor phase, simulating the accident conditions of the late phase of a LOCA sequence. The simulation of the stages of two-phase depressurizing and final quenching by reflooding will not be possible. If decided, this additional option would be of minor incidence both on planning as on cost.

5. Upgraded performance of the CABRI core

The technical requirements which must be fulfilled by the future CABRI facility include the upgrading of the driver core in order to improve the energy deposition capabilities.

Reaching the fuel rod failure is obviously a basic goal if new safety criteria, which must describe the failure risk at high burnup, have to be established.

If this postulate is used for the definition of the project goal, it is to be stated that the present knowledge of the potential failure modes is not sufficient to quantify the requirements.

Like formerly for fresh and low burnup fuel, the conservative reaching of the DNB (e.g. significant overpassing) can be used as a substitute for the failure criterion. Parametric computer calculations and experience from NSRR testing indicate that this goal will be reached at ~ 80 - 90 cal/g. So, at high burnup the design option to reach 100 cal/g should be adequate. Compared to the presently available performance this value implies an improvement of + 30 %.

A detailed neutron-physical and core behaviour study has been undertaken in view of optimizing the present core design. The result of this study is very positive as it indicates that the goal of 30 % gain can be reached with only minor modifications (see Fig. 19 and 20) :

- ① Increasing the maximum pressure of He-3 in the transient rods from 12 to 15 bars improves the peak energy deposition by a value of + 15 %.
- ② Adding one additional valve into the He-3 depressurization circuit, optimises the energy deposition in the "slow" pulse mode. The estimated gain will be of about 10 %.
- ③ Reducing the hot-spot factor of the core will improve sensibly the core performance. This factor has the high value of 1.45 in the present core and only a very few rods are responsible for this situation. The overheating rods of the core would be removed and replaced by pins with lower enrichment. The gain of this operation is not evaluated presently.
- ④ Finally it is possible to improve the neutronic coupling between the driver core and the test fuel by reducing the thickness of the presently not optimized nickel-filter which surrounds the experimental cavity (cell) inside the core. The expected improvement from this contribution is of 10 to 15 %.

The above presented modifications can be realized in parallel with the site-work during the installation of the pressurized-water-loop.

6. Tentative test-matrix and planning

This program will be undertaken most probably in international cooperation. A large discussion must therefore take place in order to formulate the test matrix and to define and select the test fuel. For the definition and as a starting point for future discussions the following guide-lines could be used :

- demonstrate the safe behaviour of conservatively selected fuel sections under plant typical conditions.
- determine failure thresholds as a function of burnup, determine failure modes and establish the basis for the evaluation of the potential consequences resulting from post-failure phenomena.
- perform a limited investigation to study defect fuel behaviour (water-logged, ...).

The test matrix must finally reflect the goal to reach the mechanistic understanding of the influence of the major parameters :

- fuel type (UO₂, MOX),
- burnup (e.g. 50, 60 and 70 GWd/t),
- energy deposition,
- pulse width.

A number of 10 to 15 experiments will be needed to reach the project goals. The matrix in table 2 gives a first indication how the tests could be defined.

The experimental program phase could start 25 months after the decision to engage the financing of the loop fabrication. The number of annual tests should not exceed 4 tests, so a 3 to 4 year period must be considered for solving most of the presently open issues.

Table 2 - Tentative CBE* test matrix

	50 GWd/t	60 GWd/t	70 GWd/t
Energy deposit.			
low	x	x	x
high	x	x	x
Fuel, Design			
high corrosion	x	x	x
low		x	x
Pulse width		x	x
Defect fuel	x	x	x

* CBE = Cabri Boucle Eau (Cabri Water Loop)

7. Conclusion

When high burnup fuel is subjected to the rapid power transients which are typical for the conditions of the design basis reactivity accidents of LWRs, a higher risk for failure and post failure fuel dispersion can be expected compared to fresh or low burnup fuel.

Fission gas driven fuel swelling and the RIM effect increase the transient loading of the cladding while corrosion phenomena decrease its mechanical resistance. The hydrogen embrittlement of the ZIRCALOY cladding, in particular the risk of local hydride accumulations (blisters), seems to be the most preoccupying aspect.

These experiments however suffer from some lack of representativity. Clad temperatures which are typical for the reactor situation cannot be achieved.

The too low temperatures can be used as arguments both for postulating excessive conservatism as for the opposite. Indeed, cold cladding produces high contact pressures but at high temperature the cladding loses rapidly its mechanical strength.

The high system pressure in the reactor might influence the fission gas behaviour and most probably the failure mode and the post failure events which include the risk of energetic fuel coolant thermodynamic interaction.

The available test facilities NSRR and CABRI will not allow to resolve completely the open questions. A project study has been performed in order to investigate the possibility to install into CABRI a pressurized-water-loop and to quantify the achievable performance characteristics of this new facility.

The result of this study allows to demonstrate that all requested technical requirements can be achieved :

- representative thermal-hydraulics (pressure, temperature, flow),
- large flexibility for the power ramp-rate (adjustable pulse-width),
- high energy deposition at high burnup (more than 100 cal/g at 65 MWd/t).

In this facility it will be possible to perform experiments which will allow to define new safety criteria and to test improved fuel at high burnup and under representative conditions.

8. Acknowledgements

The authors acknowledge gratefully the staff of the DRS-services in particular J.C. Nervi and Ch. Döderlein and the important contributions from the CEA/DRN-laboratories (D. Lespiaux and J. Noirot).

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RIA data base as a function of burnup showing fuel rod failures by high temperature and PCMI mechanisms.

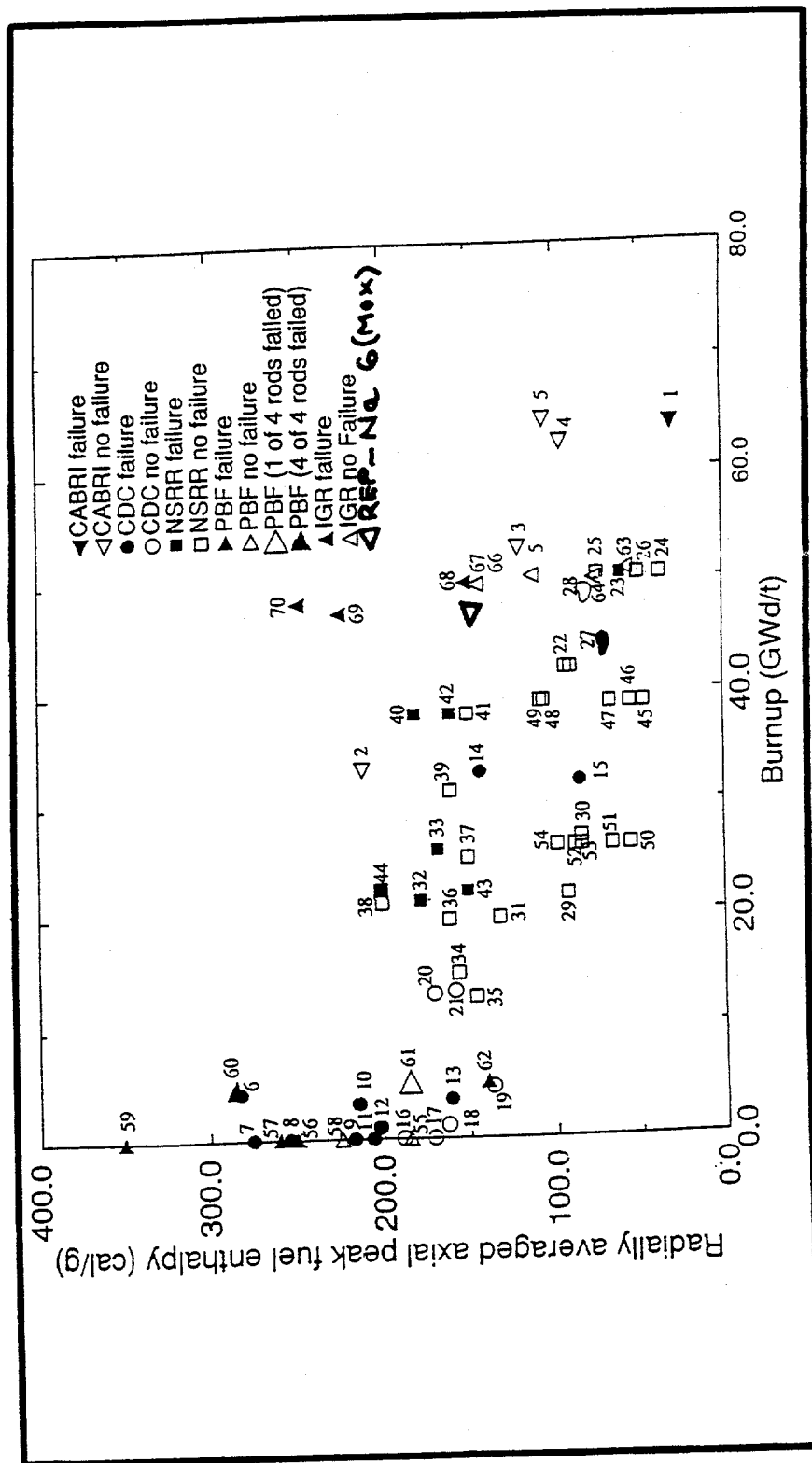


FIGURE 1 The apparently broad RIA data base is confusing because listed experiments differ in important parameters which are not accounted (rod design, t_0 state, pulse-width)

From R.K. McCardell and R.H. Hobbins (INEL)
ACRS Meeting 10/4/96



FIGURE 2 SEM picture exhibiting 3 distinct radial regions of hydrogen induced embrittlement of the high burnup cladding

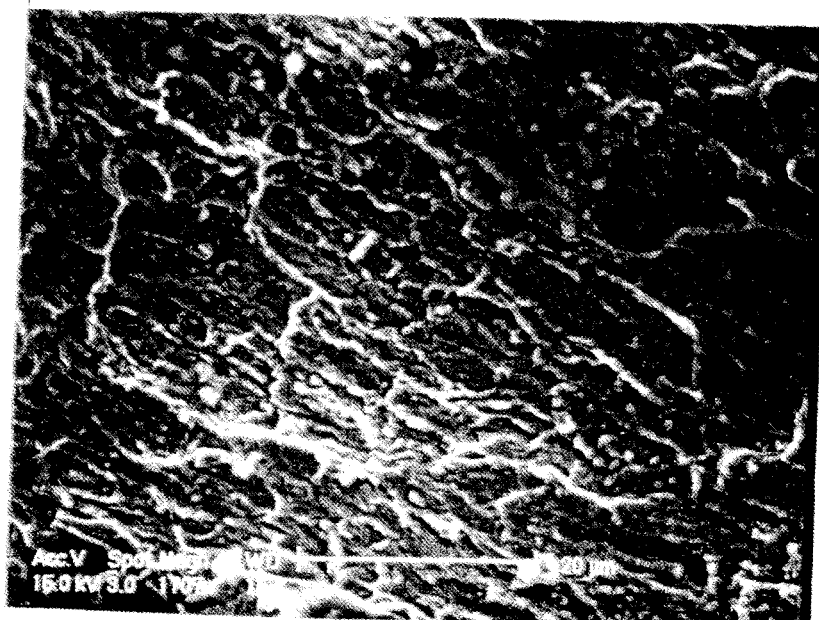


FIGURE 3 SEM picture giving hints for cleavage through hydride deposits, complicating the metallurgical diagnostic of failure mode and propagation

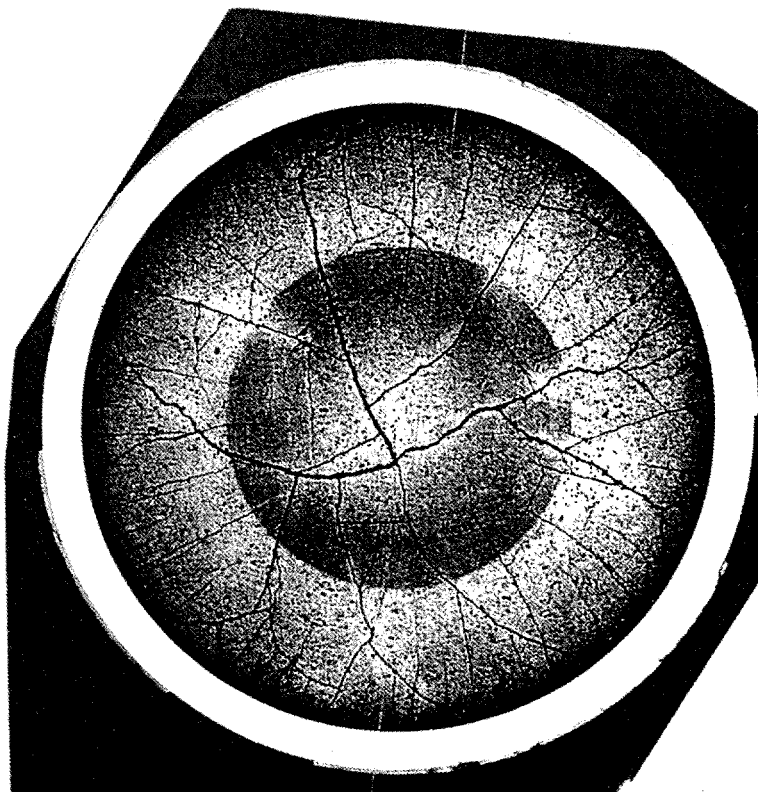


FIGURE 4 REP-Na4 macrograph after chemical etching showing a relatively narrow thermally affected outer fuel region to be explained by the larger power pulse

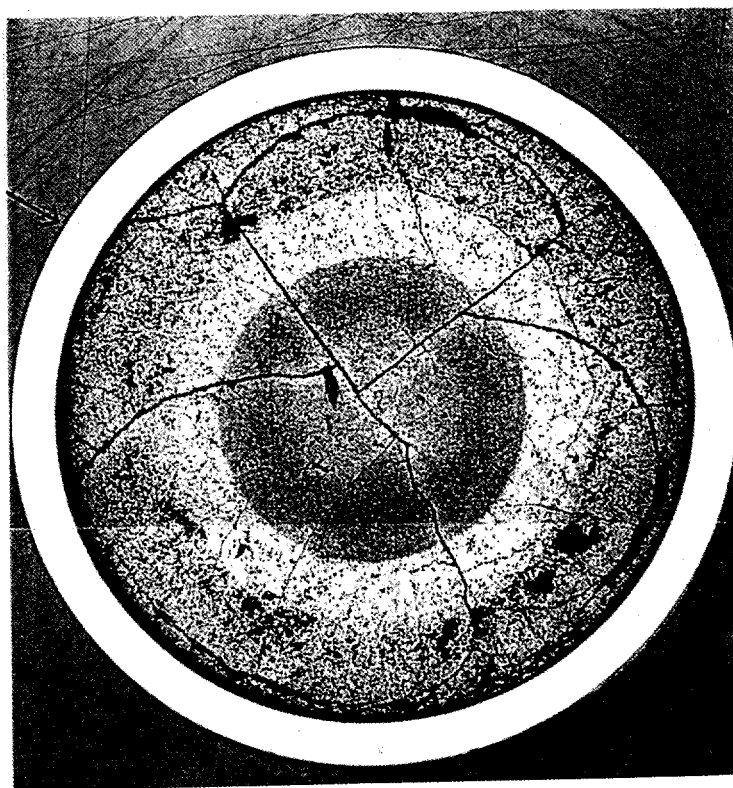


FIGURE 5 REP-Na5 macrograph after chemical etching showing a broad thermally affected outer fuel region to be explained by the higher temperatures reached with a narrow RIA power pulse

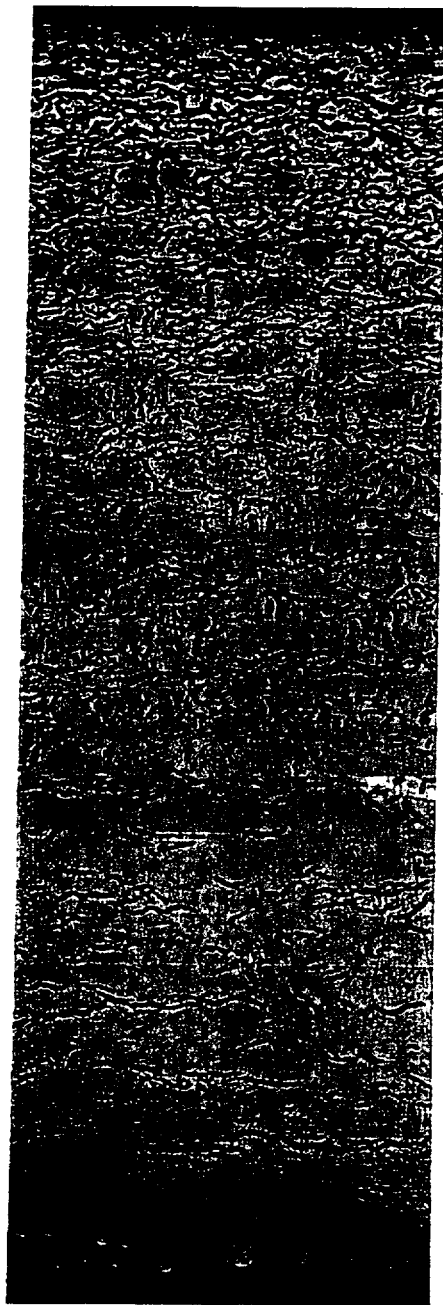


FIGURE 6

Hydride precipitates over the full section of the 5 cycle fuel rod section (span 5) after REP-Na4

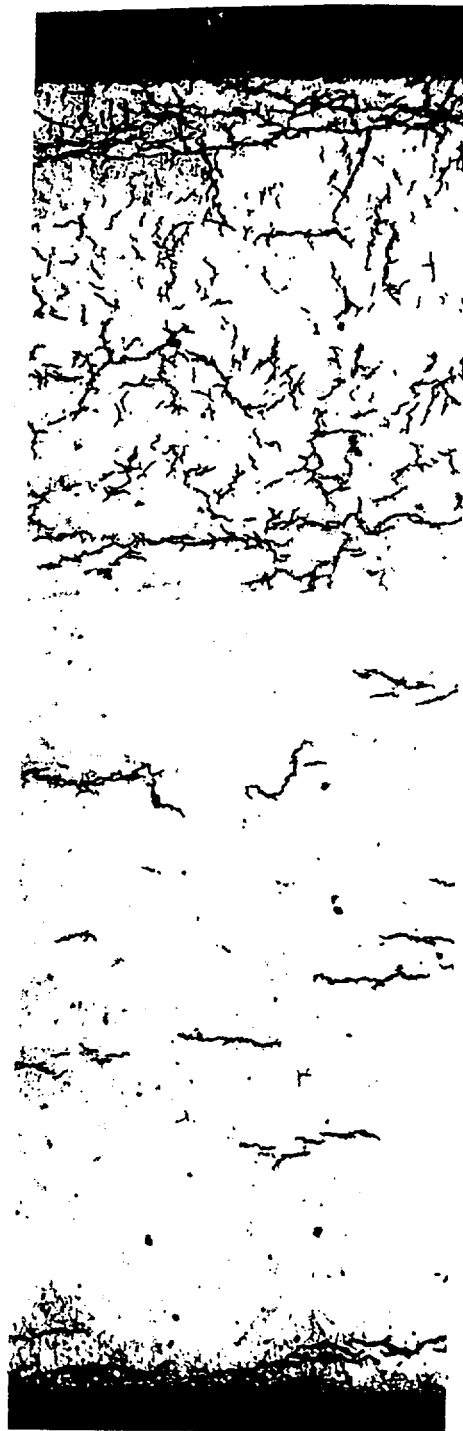
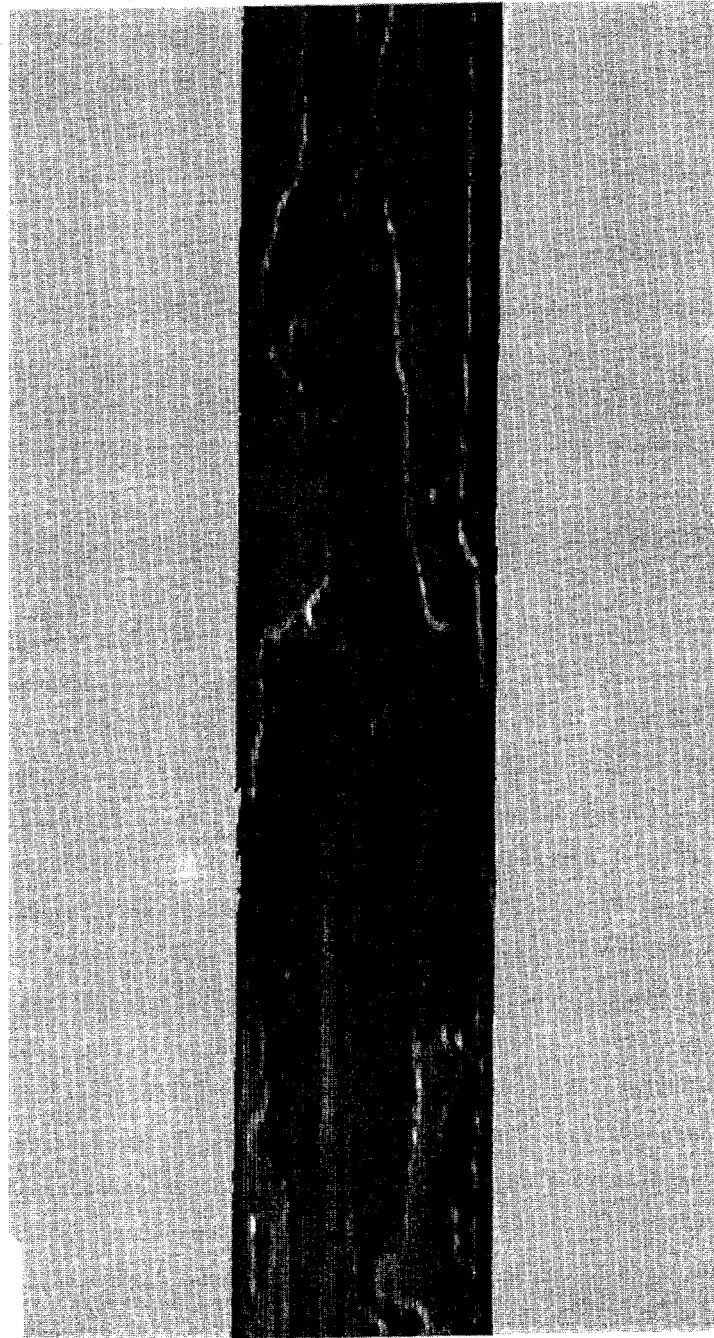


FIGURE 7

Hydride precipitates over the full section of the 5 cycle fuel rod (span 2) after REP-Na5

REP - Na 4 Test rod
(Visual inspection after test)



Aspect of ZrO_2 spalling

FIGURE 8

Oxide spalling resulting from the REP-Na4 power excursion in CABRI. Before the test, the fuel rod was covered with a regular unspalled oxide layer

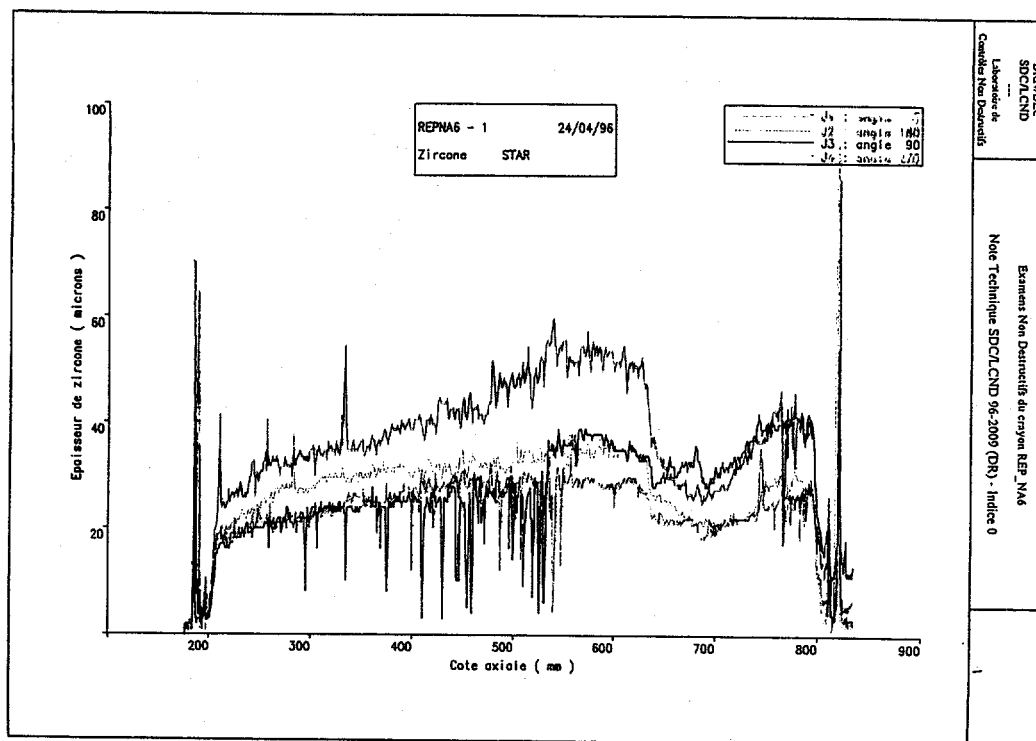


FIGURE 9

Axial ZrO_2 layer thickness distribution after REP-Na6 testing showing spalling at preferential azimuthal positions (correlated to the smaller diameter of the ovalized test rod)

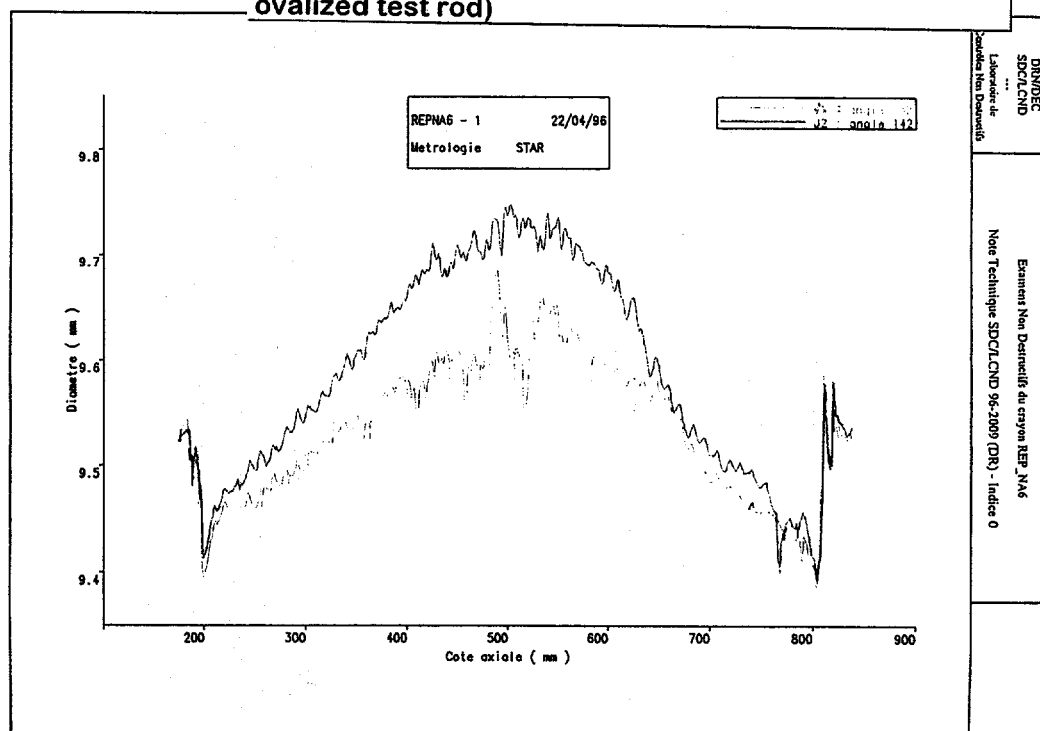


FIGURE 10

Axial distribution of 2 pin diameters at 90° of the REP-Na6 pin after testing showing important straining and ovalization

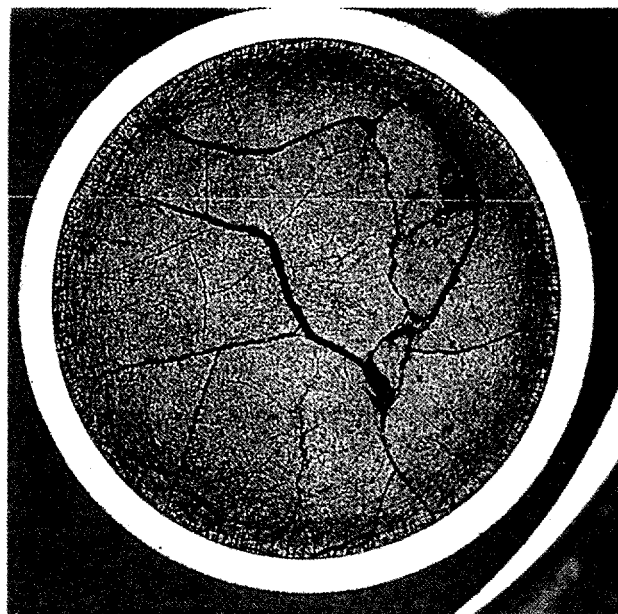


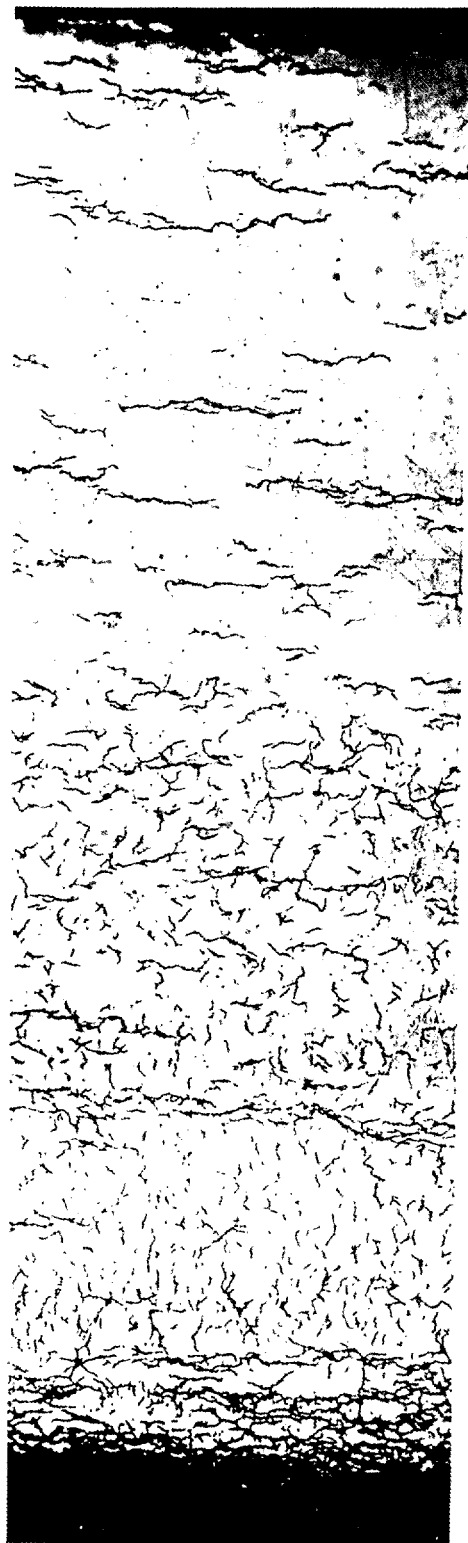
Micrographic aspects of REP-Na6. Inner and outer ZrO₂ layers and (Upu) O₂ clusters to be identified by porosity accumulations

FIGURE 11



FIGURE 12 REP-Na6 macrograph after testing (unetched) exhibiting no specific MOX phenomena. The broader cracks (compared to REP-Na4 - Fig. 4 -) are resulting from the important straining (3.2%) of the clad which is related to the high energy deposition





20 μm

FIGURE 13

Hydride precipitates over the full section of the 3 cycle MOX fuel rod (span 5) after REP-Na6

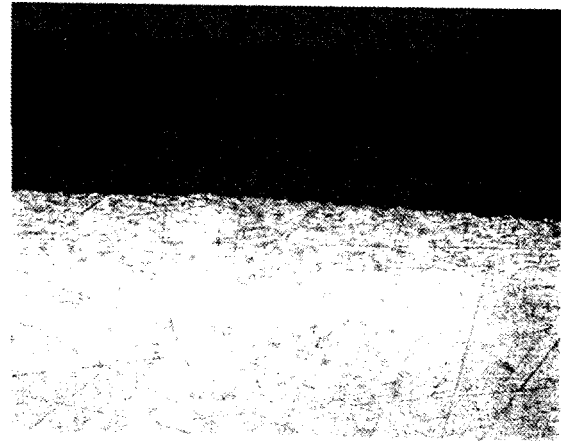


FIGURE 14

Large (Upu) O_2 cluster at the outer adge at REP-Na6 giving no indication of transient local overheating

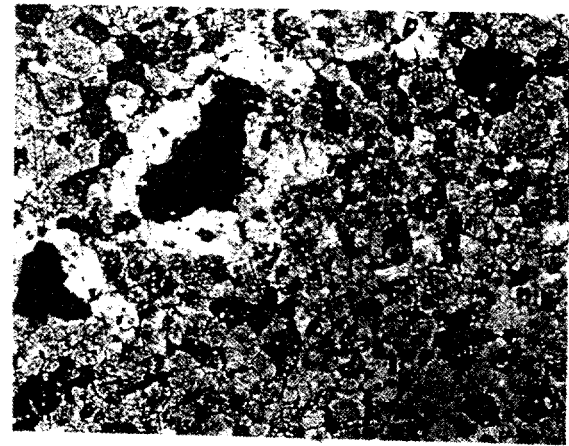
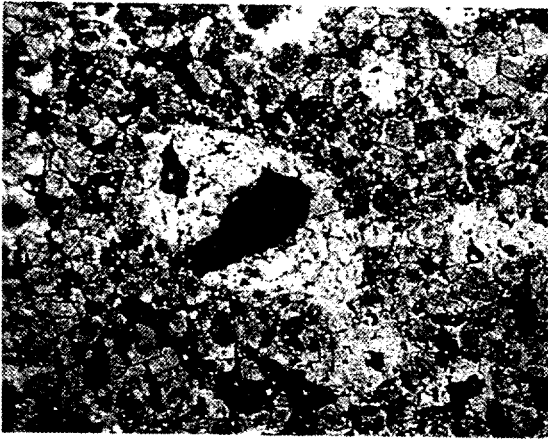


FIGURE 15

(Upu) O_2 clusters in the MOX fuel of REP-Na6 located in the central part of the fuel pellet. The important central porosity was observed already before the RIA testing



The CABRI-pressurized-water loop (PWL). Schematic presentation of the loop concept and design

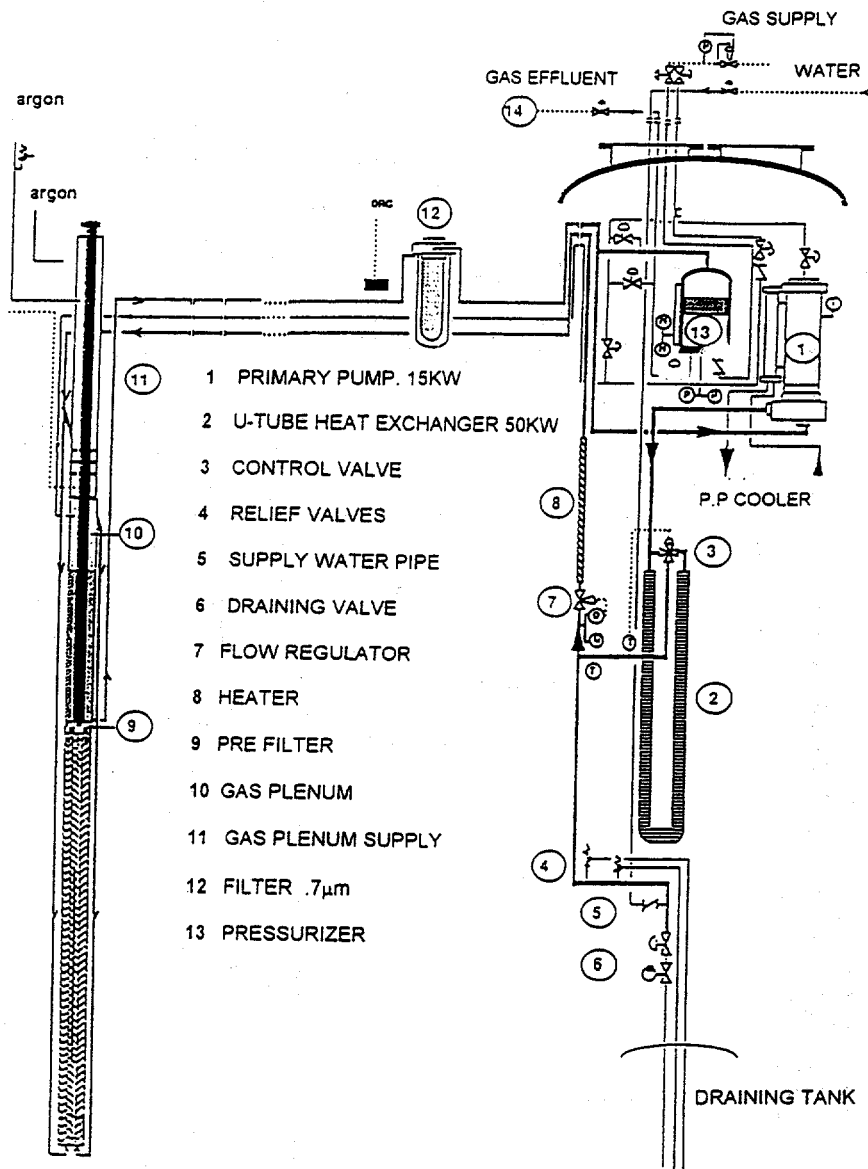


FIG 2 - PRIMARY LOOP

FIGURE 17. Detail of the CABRI-PWL presenting the primary loop

DEPARTEMENT DE RECHERCHE EN SECURITE

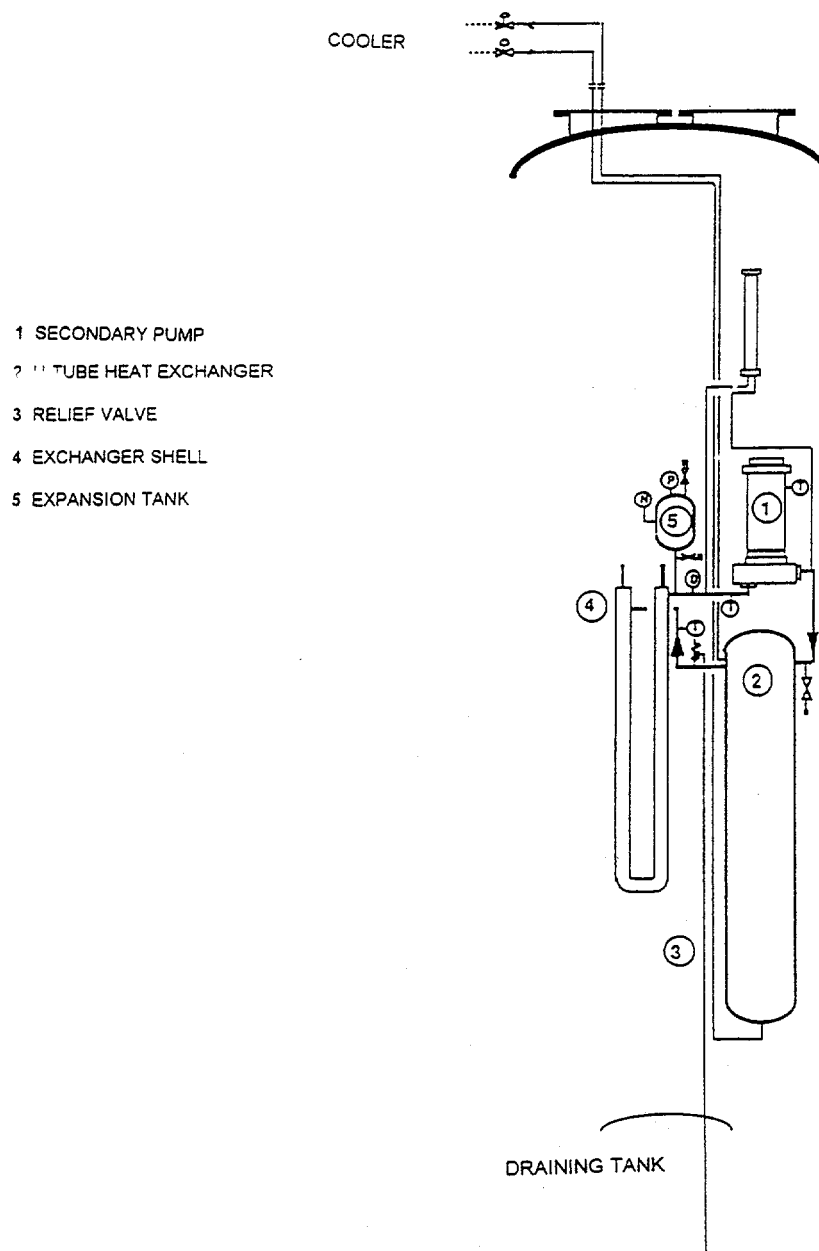


FIG 3. SECONDARY LOOP

FIGURE 18

Detail of the CABRI-PWL presenting the secondary loop

Enhancement of energy deposition

- Improvement of ^3He system
- ➡ Optimum utilization of the thermal limits in structured transients **+25%**
- Reduction of hot spot factor
- ➡ Increase of maximum core energy deposition analysis in progress
- Reduction of Nickel filter and graphite reflector thickness
- ➡ Improvement of neutronic coupling **+10-13%**

Enhancement of at least 30%

FIGURE 19

Contributions to the upgraded performance of the CABRI core improving the energy deposition capabilities by more than 30%

The CABRI Reactor Core

horizontal cut (schematic)

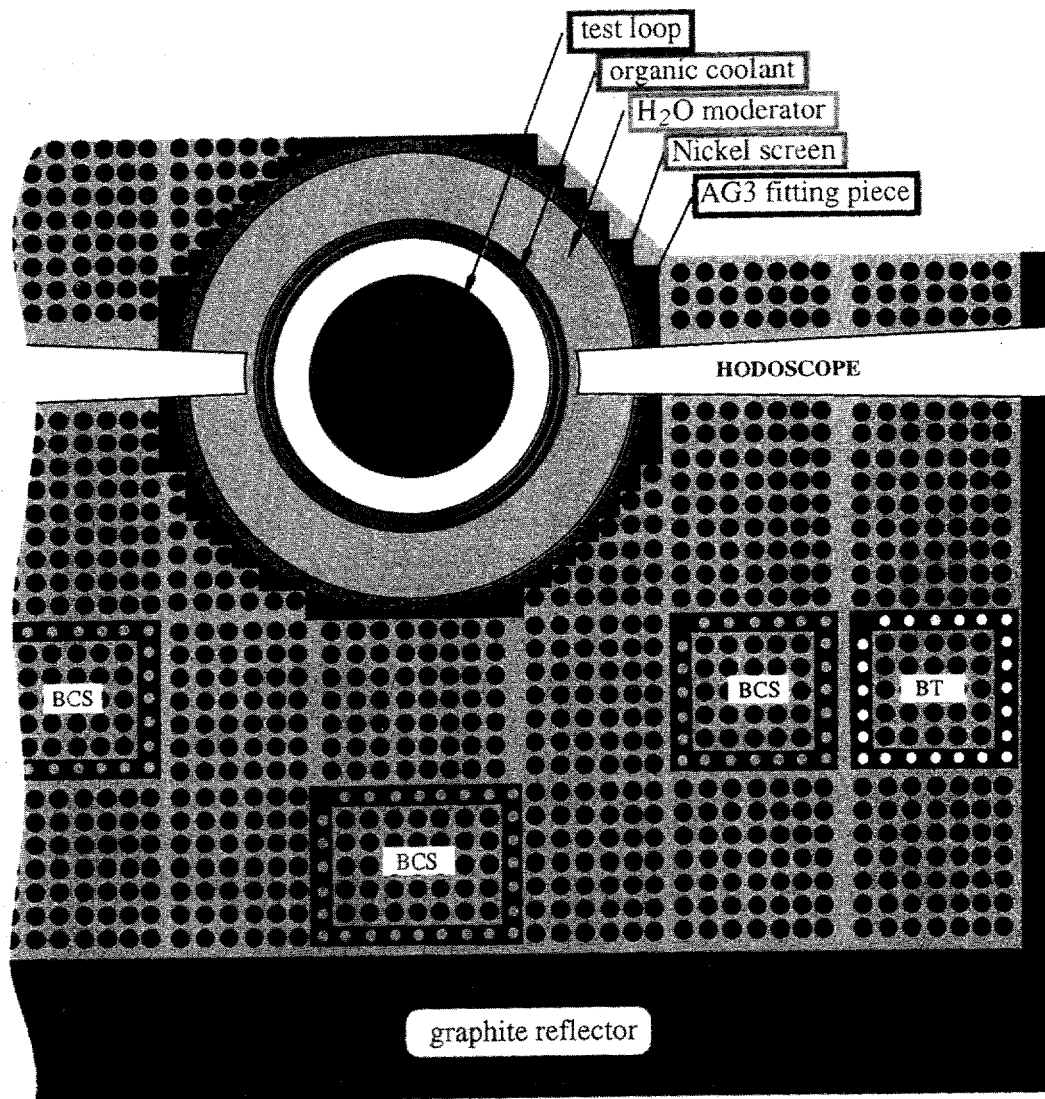
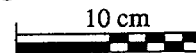


FIGURE 20

Schematic horizontal cut through the CABRI core illustrating the sites of performance optimization : N_i filter - transient rods - hot spots

RECENT RESULTS ON THE RIA TEST IN IGR REACTOR

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At the 23d WRSB meeting the data base characterizing results of VVER high burnup fuel rods tests under RIA conditions was presented. The comparison of PWR and VVER failure thresholds presented in Fig.1 was given also. Additional analysis of the obtained results was being carried out during 1996.

The results of analysis show that the two different failure mechanisms were observed for PWR and VVER fuel rods (see Fig. 2). Some factors which can be as the possible reasons of these differences are presented in Fig. 3.

First of them is the state of preirradiated cladding. Published test data for PWR high burnup fuel rods demonstrated that the PWR high burnup fuel rods failed at the RIA test are characterized by very high level of oxidation and hydriding for the claddings (see Fig.4).

Corresponding researches were performed at Institute of Atomic Reactors (RIAR, Dimitrovgrad, Russia) for large set of VVER high burnup fuel rods. Results of these investigations show that preirradiated commercial Zr-1%Nb claddings practically keep their initial levels of oxidation and H_2 concentration. Consequently the VVER preirradiated cladding must keep the high level of mechanical properties.

This year at RIAR the first measurements of mechanical properties of claddings of the VVER high burnup commercial fuel elements (burnup up to 60 MWd/kg U) were performed. The measured values of the ultimate strength for unirradiated and preirradiated Zr-1%Nb cladding are shown in Fig.5. The ultimate strength versus temperature for unirradiated Zry-4 is demonstrated also.

As can be observed from these data, the measured ultimate strength for preirradiated Zr-1%Nb cladding approximately corresponds to the ultimate strength for unirradiated Zry-4. Obtained results confirm that the cladding of VVER high burnup fuel rod possesses the sufficient reserve to resist to the PCMI loads. The illustrations of the two types of cladding failure mechanisms for PWR and VVER high burnup fuel rods are given in Fig.6-8.

As there was noted above, the second reason leading to differences between failure mechanisms for two types of high burnup fuel rods can be the test conditions. Now such kind of analysis have been performed by two methods. The first one was based on the all set of VVER RIA tests carried out at the IGR and GIDRA reactors. The results of this approach are shown in Fig.9.

These results demonstrate that the lowest failure threshold of different VVER fuel rods was fixed for high pressurized fuel rods. It is significant that the failure threshold of high burnup fuel rods is equal to the failure threshold of fresh fuel rods. An additional point to emphasize is that the influence of pulse half width on the failure threshold for VVER fresh fuel rods (in the range of 2 ms - 1 sec) was not revealed. By these means the PCMI mechanism was not observed at the VVER RIA tests. Nevertheless the careful analysis of test conditions shows that VVER high burnup fuel

rods were tested at the pulse half width of about 500-700 ms. The minimum initial gas gap in the high burnup fuel rods was 30 μm .

Taking into account these factors it should be determined the type of failure mechanism and failure threshold for VVER high burnup fuel rods under tests conditions with the more short pulse and with the zero pellet cladding gap.

That is why the second method of the analysis to reveal the influence of the test conditions was used. The FRAP-T6 code was applied to assess the behavior of VVER fuel rods. Special cycle of researches to apply the FRAP-T6 code for VVER fuel rods analysis was carried out at NSI RRC KI in collaboration with NRC (USA) and IPSN (France). The example of current status of these researches is demonstrated in Fig.10.

The presented test and calculated results allow to make preliminary conclusions that although many problems to describe test results should be decided, nevertheless, the scale of the maximum cladding deformation is simulated by FRAP-T6 code quite adequately. From this the FRAP-T6 code base some calculations were made to determine the influence of the pellet cladding gap and the pulse half width on the maximum hoop stress in the VVER cladding. The results of these calculations are presented in Figs.11-12.

The comparison of obtained results with ultimate strength shows it is possible to get the PCMI failure of VVER high burnup fuel elements at the pulse half width less than 50 ms and the gas gap less than 30 μm . It must be admitted that these results are very preliminary because now there is not a good data base with Zr-1%Nb mechanical properties for such kind of analysis. And this is the prime task for the immediate researches.

Briefly summarizing all that was described above it may be inferred that:

1. RIA test results have demonstrated that PWR- and VVER high burnup fuel rods are characterized by different failure mechanisms and failure thresholds.
2. IGR test results have shown that the peak fuel enthalpy at the failure does not practically depend on the pulse half width (in the range of 2 ms - 1 sec) for VVER fresh fuel rods and on the burnup for VVER fuel rods tested at pulse half width about 700 ms.
3. Mechanical properties of preirradiated claddings are key factors to predict the fuel element failure parameters under RIA conditions.
4. Such test conditions as the initial gas gap, the pressure drop on the cladding and the pulse half width can lead to change of the failure mechanism and threshold at the specific sets of their values.
5. Special out-of-pile tests and computer calculations should be performed to adjust all set of boundary conditions provoking the PCMI failure at the low enthalpies.

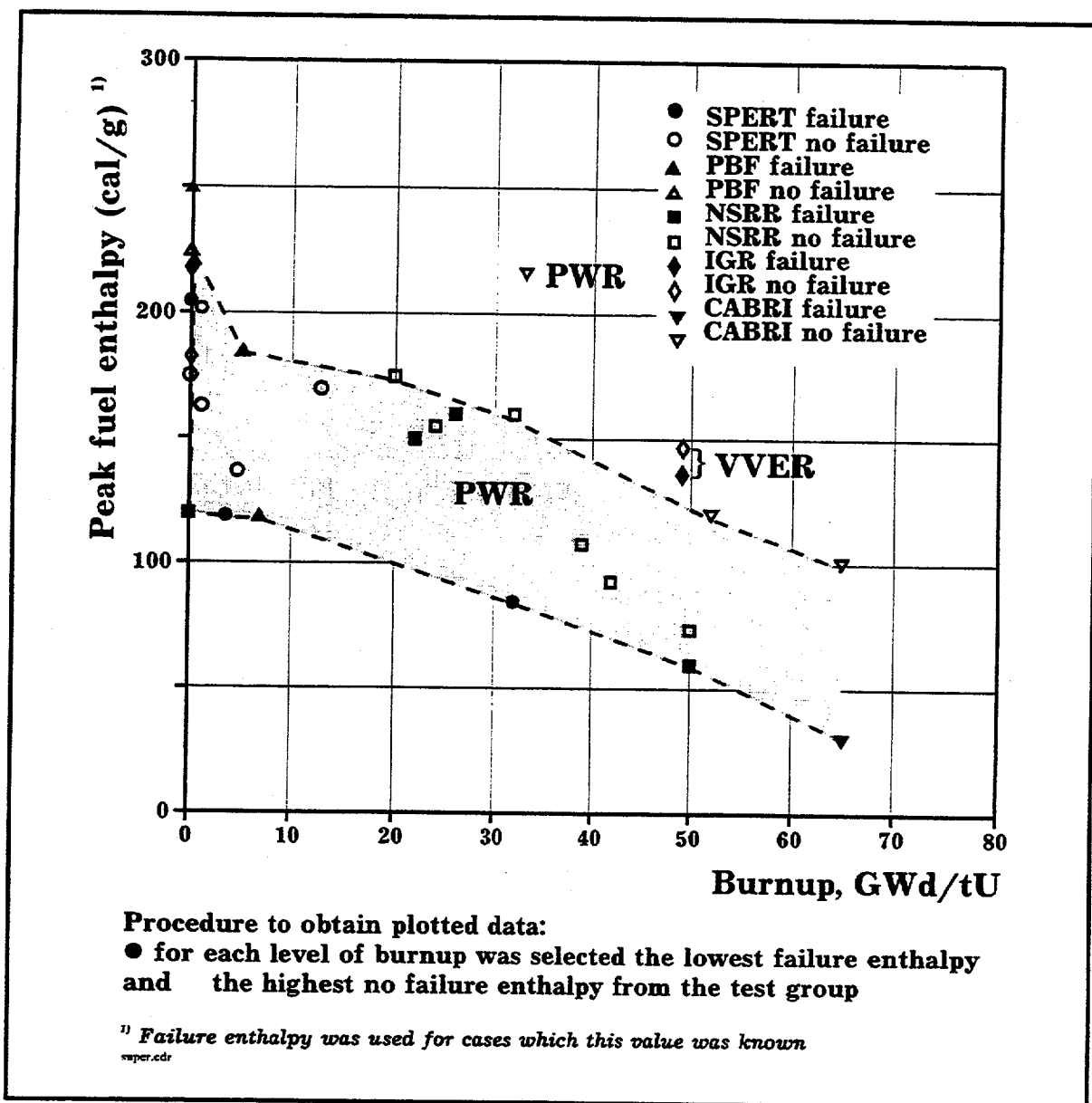


Fig.1. Region of threshold failure fuel enthalpy for PWR and VVER fuel rods plotted versus burnup under RIA conditions

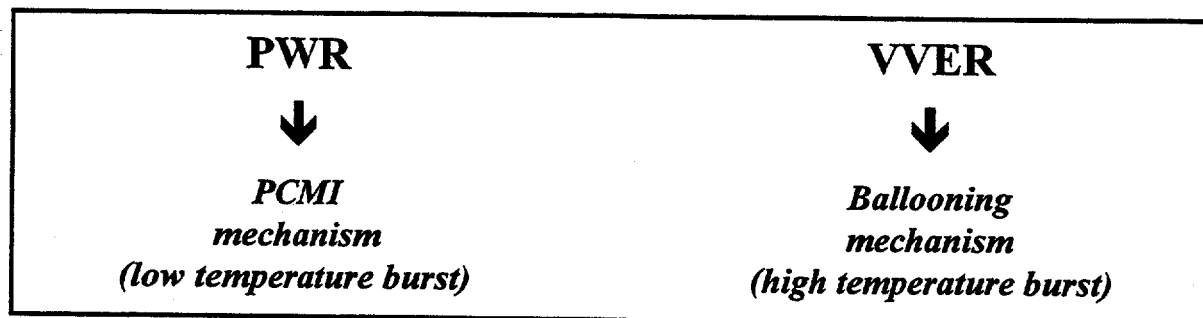


Fig.2. Typical failure mechanisms for PWR and VVER high burnup fuel elements under RIA conditions

Mechanical properties of preirradiated cladding	Test conditions: • pressure drop on the cladding VVER • pellet-cladding gap • pulse half width
---	--

Fig.3. Possible reasons leading to differences between VVER- and PWR RIA test results

PWR ↓	VVER ↓
<i>1. Oxidation up to 100 μm.</i> <i>2. Hydride concentration up to 800 ppm.</i>	<i>1. Practically it is no influence of burnup on the cladding oxidation and hydride concentration:</i> • oxidation up to 3-5 mm • hydride concentration up to 50 ppm

Fig.4. Some typical characteristics of PWR and VVER claddings at the end of fuel operating cycle

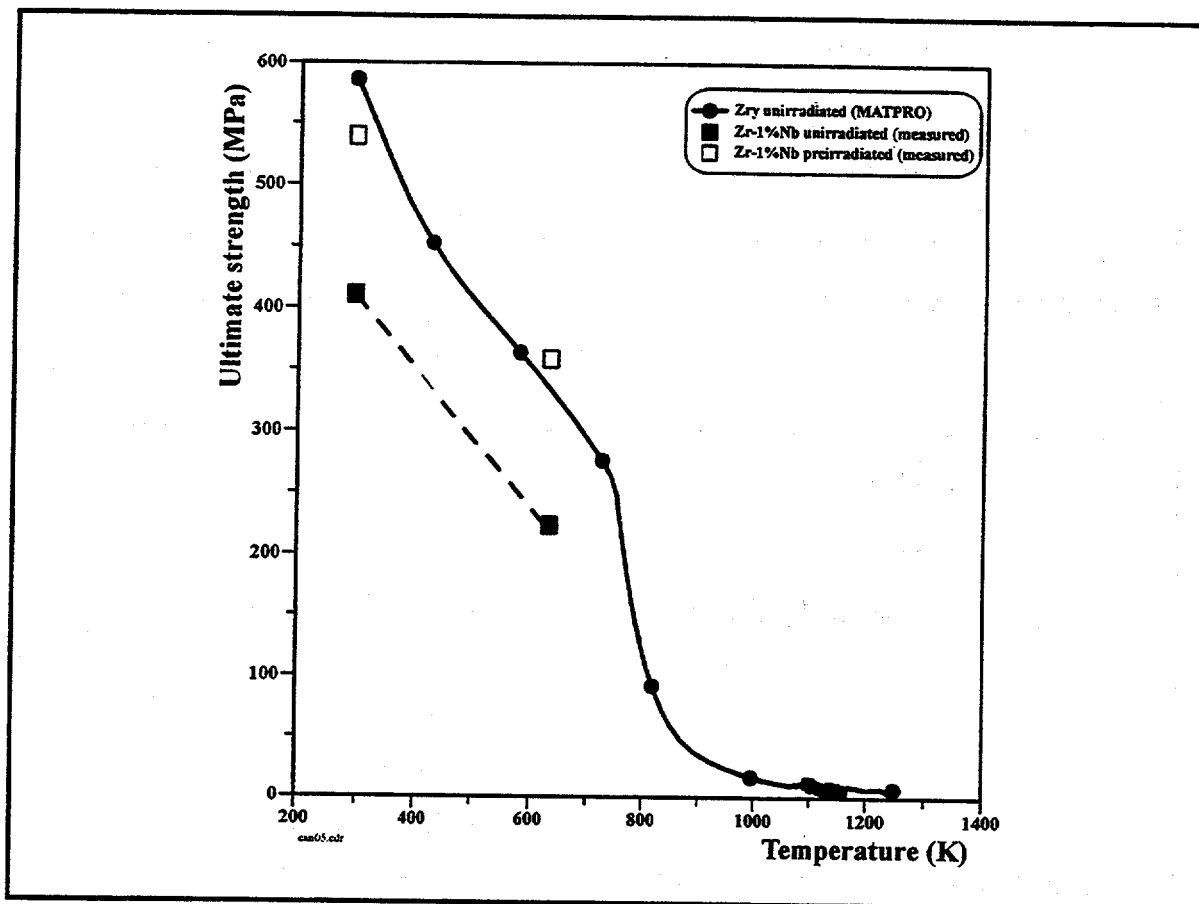


Fig.5. Ultimate strength for PWR- and VVER cladding materials

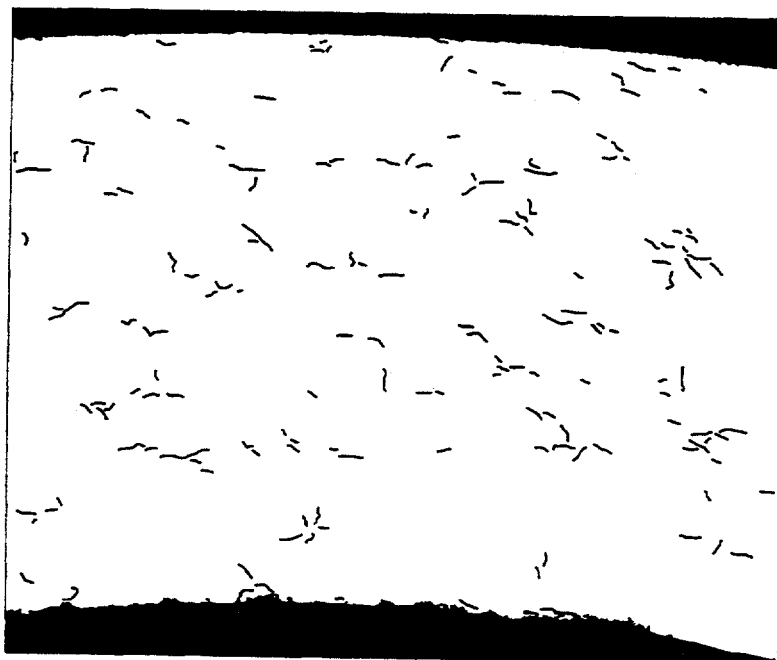


Fig.6. Hydride distribution in the cladding of VVER fuel element preirradiated up to 50 MWd/kg U at NV NPP

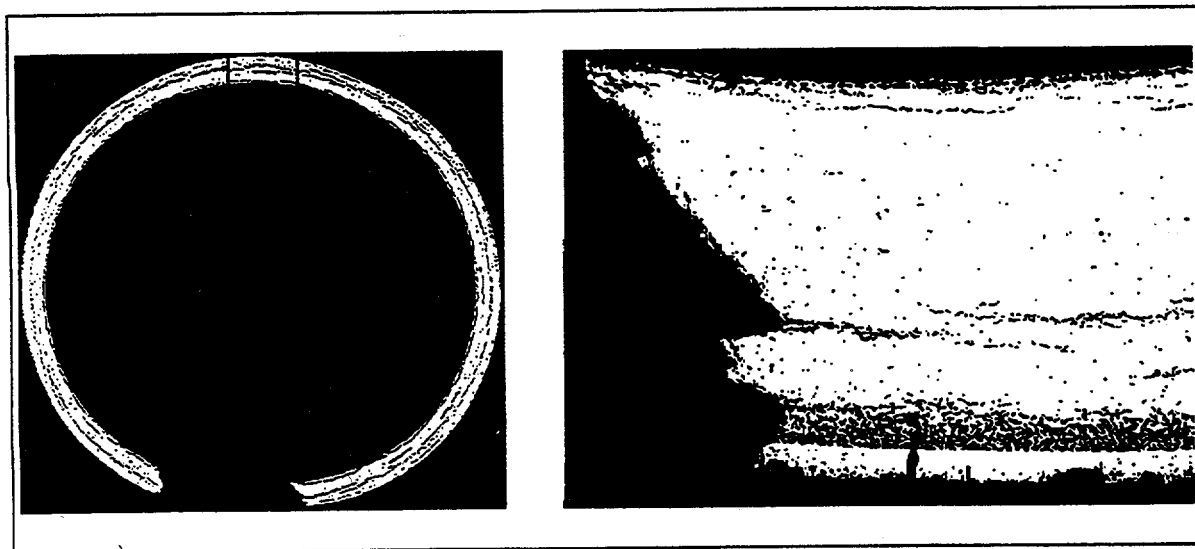


Fig.7. Appearance of cladding failure after HBO-1 test (NSRR, Japan)

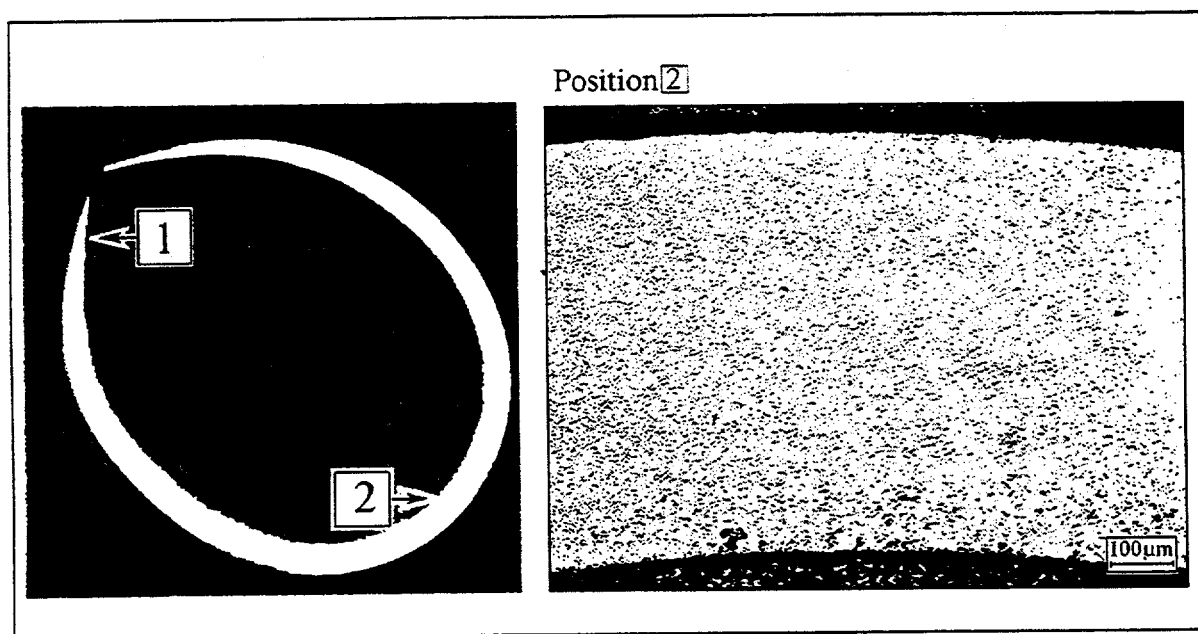


Fig.8. Cross-section and cladding microstructure for fuel rod #H7T at 158 mm elevation after IGR test

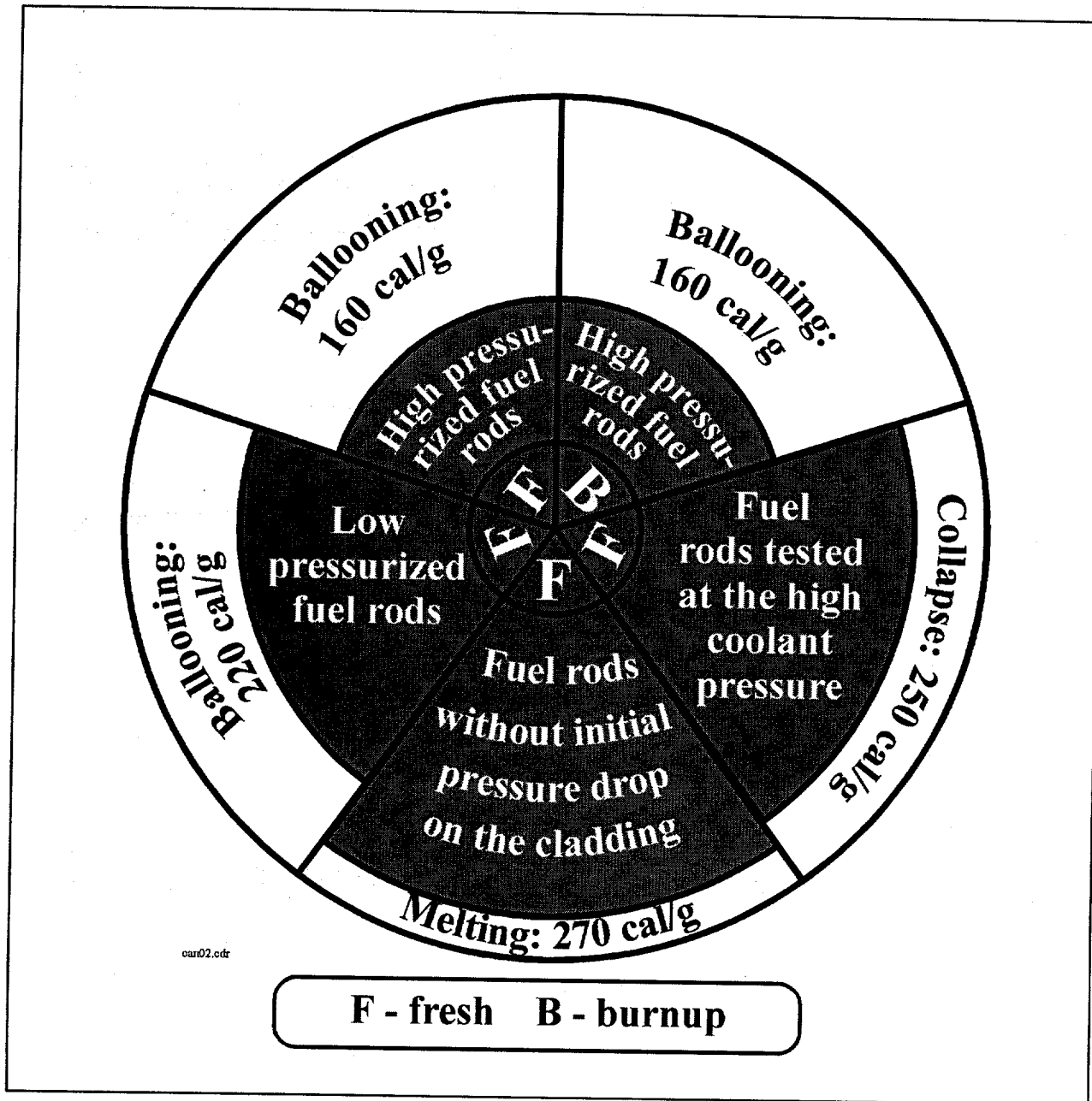


Fig.9. Results of VVER RIA tests: failure mechanisms and failure thresholds

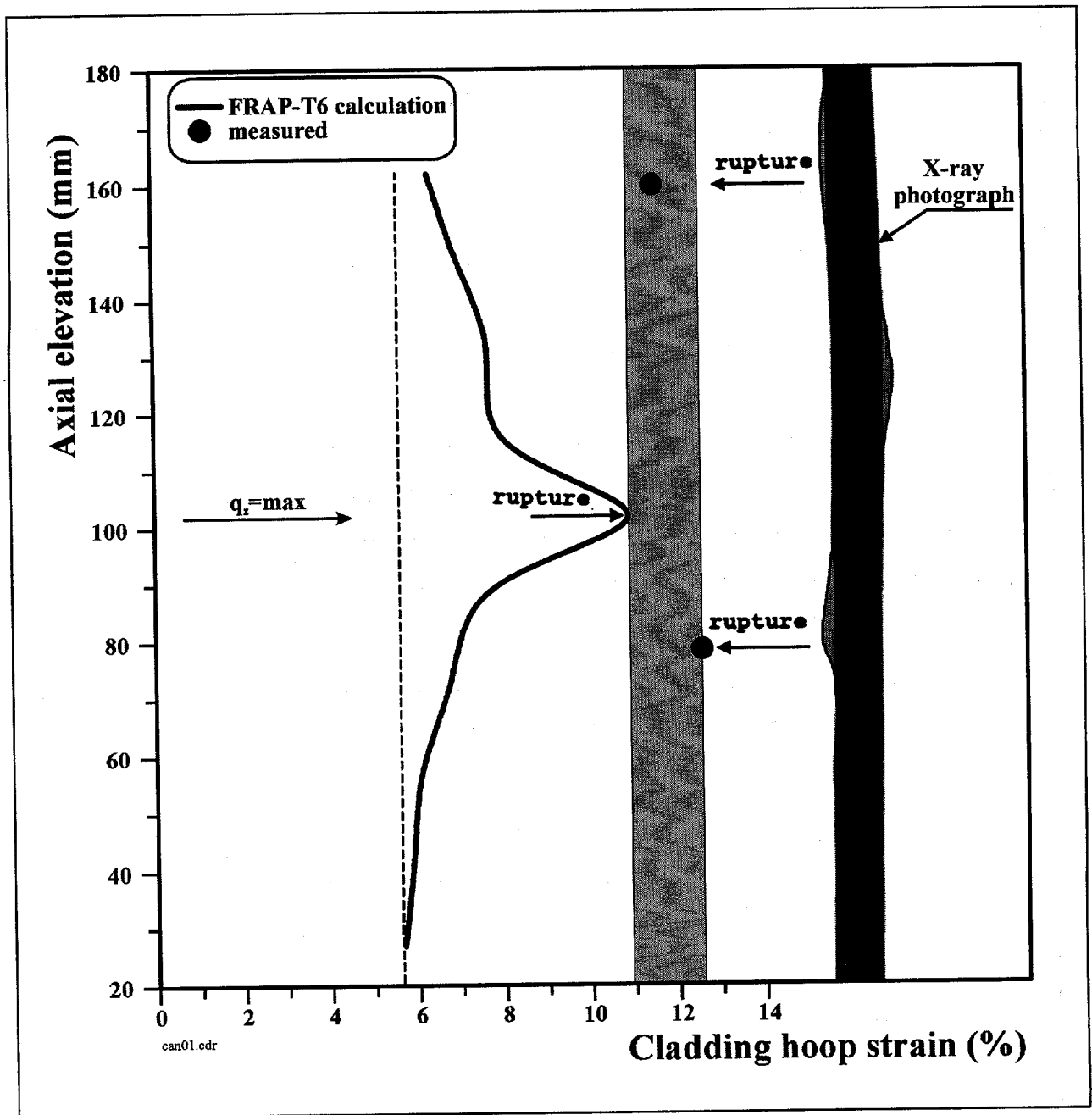


Fig.10. Test and calculated results characterizing the failure mechanism for VVER high burnup fuel rod #H2T

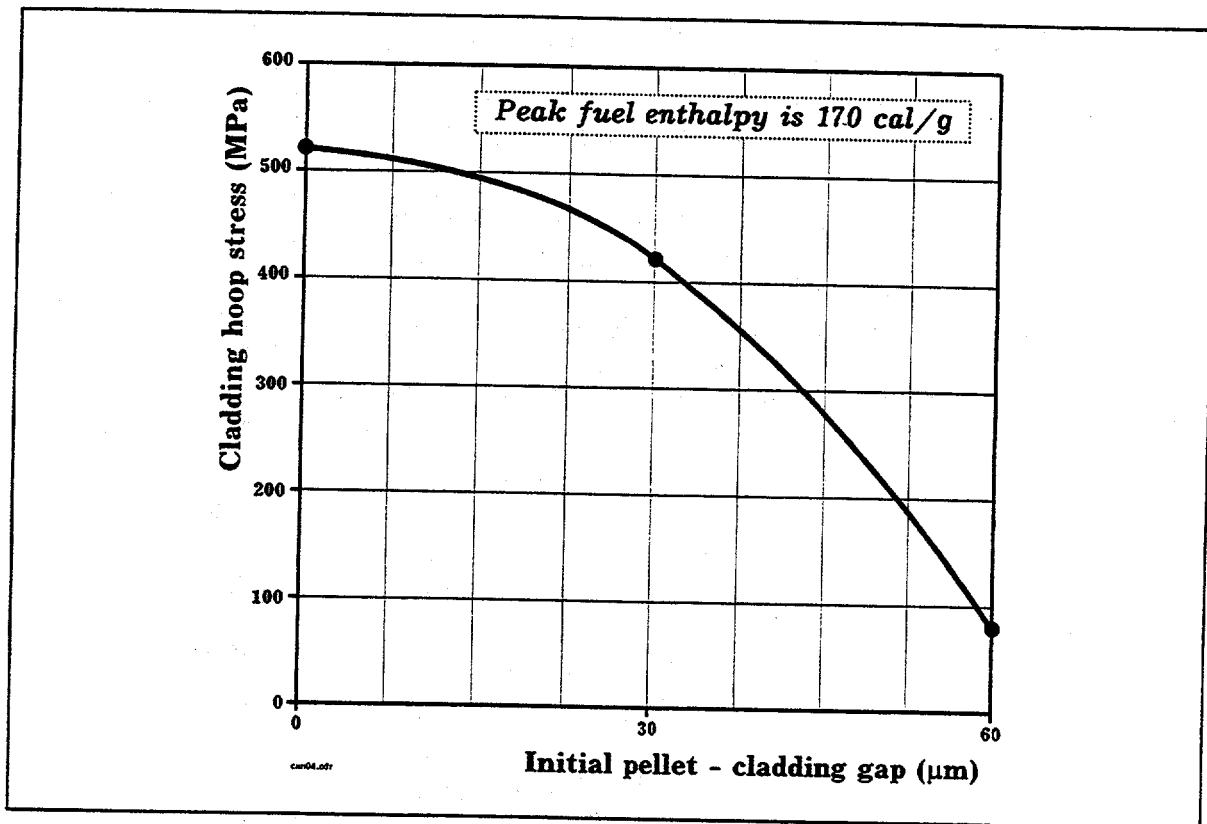


Fig.11. Maximal cladding hoop stress vs initial pellet - cladding gap calculated by FRAP-T6 code for VVER fuel rod #H5T

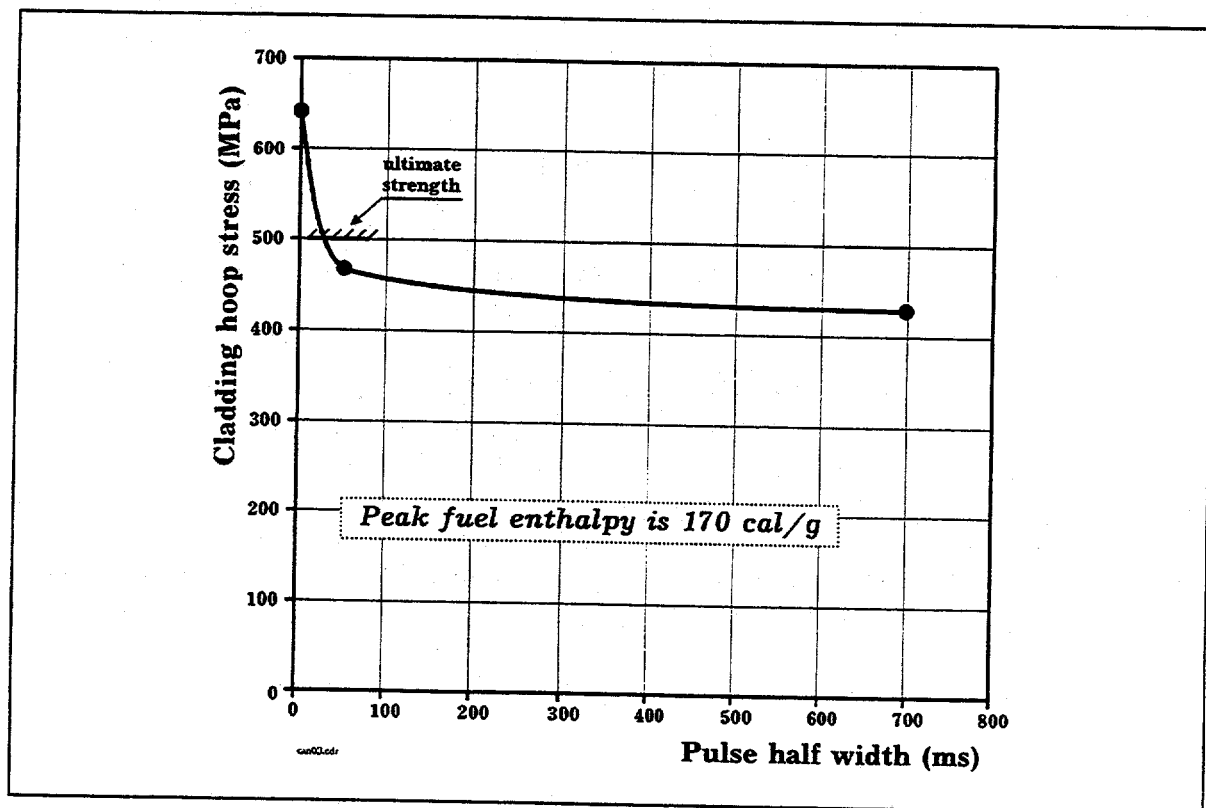


Fig.12. Maximal cladding hoop stress vs pulse half width calculated by FRAP-T6 code for VVER fuel rod #H5T

On the Issue of Zircaloy Ductility during a Reactivity-Initiated Accident

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Abstract

During reactor exposure, Zircaloy cladding undergoes various microstructural changes including irradiation damage, oxidation, and hydrogen pick-up. There is a concern that the combination of these changes in high burnup cladding will cause failure during a reactivity-initiated accident (RIA) at an energy deposition level significantly lower than that of fresh cladding. In RIA conditions, the cladding must withstand loading at high strain rates and under deformation paths close to transverse plane-strain extension. Thus to assess cladding failure it is necessary to examine the failure mechanism of unirradiated Zircaloy cladding under RIA-like loading conditions. We present here a theoretical analysis of a possible failure mode of Zircaloy cladding due to localized necking. The results of the analysis suggest that high-burnup cladding is susceptible to pronounced losses of ductility under a combination of plane strain loading deformation and the presence of thickness imperfections. Such imperfections may be caused by hydride embrittlement of the cladding or non-uniform oxidation such that an axial thickness change is created.

1. Introduction.

In today's nuclear power reactors, zirconium alloys are commonly used for fuel cladding and structural components because of their combination of low neutron absorption, superior high temperature corrosion resistance, and adequate mechanical properties. There is a substantial body of in-reactor experience with these alloys, especially Zircaloy-2 and Zircaloy-4 used in BWRs and PWRs, respectively. The in-reactor performance of these alloys has been good, allowing the cladding to fulfill its primary function of containing the fission products and providing structural integrity to the fuel rods under normal operating conditions [1].

In an effort to increase the capacity factor of nuclear power plants, utilities and fuel vendors are currently proposing to increase fuel burnup from the current 30 GWd/ton to 45 and 60 GWd/ton, while also increasing the fuel cycle from 12 to 18 and 24 months. These changes will help reduce fuel outages and allow nuclear energy to be more competitive. However, the proposed burnup increase will double the exposure time of the cladding in the reactor environment, where it is exposed to concomitant neutron irradiation, oxidation and hydriding. It is mandatory that the fuel continue to operate safely under these more demanding conditions, both under normal operation and accident conditions.

One of the postulated analysis accidents is the reactivity-insertion accident (RIA), caused by a control rod drop or control-rod ejection. Recent full-scale simulations conducted at the CABRI reactor in France, the NSSR reactor in Japan and at the Kurchatov Institute in Russia [2] appear to show that, as the burnup increases above 45 and 50 GWd/ton, there is a decrease in the ability of the cladding to withstand the large energy deposition attendant upon an RIA. If the cladding fails during an RIA, it is possible that fuel dispersal could cause flow blockage and an unacceptable rise in fuel temperature. This raises concerns about the extension of fuel burnup levels beyond current limits.

In order to properly evaluate the possible consequences of this accident on reactor cladding, it is necessary to understand the behavior of Zircaloy under RIA-like loading conditions (plane-strain loading, high strain rate), on a cladding that has suffered oxidation and hydriding, in addition to radiation damage. We report here a preliminary analysis of a localized necking instability as a possible failure mode during an RIA event. It is well known that sheet metal ductility is usually limited by a localized necking instability. During localized necking, through-thickness slip is concentrated in a narrow band of material such that the adjoining material ceases to deform. Therefore, the overall ductility of the material may be severely limited even though the fracture occurs in a ductile manner. This type of instability can be triggered by small imperfections in thickness, caused for example by non-uniform oxidation or by the failure of a thin rim of embrittled material at small strains. High burn-up thin-wall cladding and exhibiting low work-hardening should be especially sensitive to this form of instability, especially if forced to deform under plane-strain conditions. Hence, this model is very relevant to loading and deformation of high burnup fuel cladding due to a RIA event.

2. Failure of High-burnup Zircaloy Cladding under RIA conditions.

One of the consequences of a reactivity-initiated accident is a large instantaneous energy deposition in the fuel. The resulting increase in fuel and cladding temperature can lead to fuel rod failure and, in more severe cases, to fuel dispersion and loss of coolable geometry. Based on simulation experiments conducted in the 1970's in the US [3], regulatory guidelines were established that set energy deposition limits to avoid fuel rod failure and loss of coolable configuration (due to fuel dispersion). The thresholds for failure and for fuel dispersion were established at 170 and 280 cal/g UO_2 , respectively.

Recent results appear to indicate that the fuel failure threshold decreases significantly as the burnup increases [2]. In experiments performed in the CABRI facility in France [4], Zircaloy-4 cladding irradiated to 65 Gwd/ton failed at a peak averaged fuel enthalpy of 30 cal/g. Failure of the cladding wall occurred due to crack growth with little or no thinning of the cladding wall and on a plane normal to the cladding surface (i.e. radial cracks).

In contrast to the above, for the HBO experiments conducted in the NSSR facility in Japan, presented a different failure mode [5]. Occurring at lower temperatures ($\sim 50^\circ\text{C}$) and much lower overall hydrogen content (~ 190 ppm average), failure in the HBO-1

test is in some ways of greater safety concern. First, cladding failure occurred despite some plasticity of the cladding and despite cladding wall thinning. Second, failure of the cladding has a much different fracture profile than the CABRI experiment. Post irradiation examination showed the cladding has many surface cracks (< 0.1 mm long) which arrested within the ductile cladding. Unlike the CABRI failure, most of the fracture surface involves shear on planes often inclined $\pm 45^\circ$ through the thickness. Thus, most of the HBO-1 cladding has good crack growth resistance with failure apparently dominated by plane stress and through-thickness slip. Hydrides are evident only near the outer surface (within $\sim 50\mu\text{m}$) of the cladding.

It is significant that a second test performed at the NSSR facility, (HBO-3), was performed under almost identical conditions but did not fail. The Zircaloy cladding in the HBO-3 test had a slightly thinner oxide thickness than HBO-1 (20 to $25\mu\text{m}$ compared to 38 to $48\mu\text{m}$) and a lower average hydrogen content (187 ppm vs. 148 ppm). Importantly, the outer rim of cladding which contained excessive hydrides was significantly thicker in HBO-1 than in HBO-3 (approximately $50\mu\text{m}$ as compared to $25\mu\text{m}$ in HBO-3). Under test conditions, HBO-1 failed with less than 1% hoop strain, while the cladding in the HBO-3 test exhibited approximately 1.5% residual hoop strain without failing. Thus small differences in material conditions resulted in large differences in failure responses between these two tests.

In the present paper, we draw attention to a consequence of sheet metal plasticity in which a localized necking instability can occur under certain conditions. Once triggered, such an instability can limit ductility and cause failure at very small strains, even though the failure process itself is ductile fracture characterized by through-thickness shear on a plane inclined roughly 45 degrees through the cladding thickness. As the analysis below indicates, the onset of localized necking is very sensitive to the deformation path imposed on the cladding during an RIA event, the deformation behavior of the cladding (specifically its strain-hardening characteristics), and the presence of any thickness imperfections.

Given the small thickness of Zircaloy cladding ($\sim 610\mu\text{m}$), a relatively thin strip ($\sim 50\mu\text{m}$) of hydrided material which cracks (or fails at small strains) can create an imperfection which can trigger a localized necking phenomenon, resulting in cladding failure at small macroscopic strains. We believe that the presence of a thin rim of hydrided cladding which fails at very small strains can in effect create the type of thickness imperfection which can trigger a localized necking instability which severely curtails any hoop extension of the cladding. With regard to the differing behavior of the HBO-1 and HBO-3 tests, the analysis suggests that the thickness of an embrittled, hydrided rim is a critical factor which determines whether or not cladding will fail in a RIA event.

We now show how such a mechanism can limit the ductility of Zircaloy cladding.

3. Influence of an Embrittled Rim on Zircaloy Ductility

The failure of Zircaloy cladding during pellet-cladding mechanical interaction resulting from an RIA, is usually attributed to either crack growth leading to brittle failure, or to the global accumulation of damage throughout the cladding thickness, leading to a general yielding type of failure. However, it is well known that the ductility of sheet metal is frequently limited by a plastic instability known as localized necking [6-9]. During localized necking, through-thickness slip deformation is concentrated within a narrow band of material such that the adjoining material ceases to deform. The maximum principal strain within the material at the onset of localized necking thus determines fracture strain if no other failure mechanism occurs at smaller strains. Furthermore, since final failure occurs as a result of intense through thickness deformation on planes of high shear stress, the orientation of the failure plane is $\pm 45^\circ$ to the sheet surface.

In this work, we present analysis which suggests that a thin layer of embrittled material, which fails at small strain, creates a thickness imperfection that seriously degrades the ductility of otherwise undamaged cladding. We assume that the cladding contains a layer of metal which has been embrittled due to a combination of radiation damage, hydriding and oxidation. Once this material has failed there are two regions, A and B, with thicknesses t_A^o and t_B^o , as shown in figure 1. If localized necking controls the cladding failure strain, $\bar{\epsilon}_f$, then

$$\bar{\epsilon}_f = \bar{\epsilon}_e + \Delta\bar{\epsilon}_\gamma \quad (1)$$

where $\Delta\bar{\epsilon}_\gamma$ is the equivalent strain increment to initiate localized necking once the embrittled layer fails at $\bar{\epsilon}_e$. To estimate $\Delta\bar{\epsilon}_\gamma$ we assume the material obeys the power law hardening relationship.

$$\bar{\sigma} = K \bar{\epsilon}^n \dot{\bar{\epsilon}}^m \quad (2)$$

where $\bar{\epsilon}$ is the equivalent strain, $\dot{\bar{\epsilon}}$ is the equivalent strain rate, n is the strain hardening exponent, m the strain rate hardening exponent, and K the strength coefficient. Furthermore, we assume that the cladding deforms in plane-strain tension such that its axial extension is zero (i.e. $\epsilon_{zz} = 0$ in figure 1). In assuming isotropic plasticity we recognize the inherent anisotropy of Zircaloy but also note that the onset of localized necking should be relatively insensitive to plastic anisotropy for the case of plane strain tension [7] expected to be present in this case.

From equilibrium we can write

$$\sigma_A t_A = \sigma_B t_B \quad (3)$$

Defining the severity of the flaw as

$$f = 1 - \frac{t_A^o}{t_B^o} \quad (4)$$

and using von Mises' flow theory, we can rewrite equation 3 as [8]

$$(\epsilon_1^A)^{n/m} \exp(-\epsilon_1^A / m) d\epsilon_1^A = (1-f)^{1/m} (\epsilon_1^B)^{n/m} \exp(-\epsilon_1^B / m) d\epsilon_1^B \quad (5)$$

where ϵ_1^A and ϵ_1^B are the principal strains in the A and B regions. Equation 5 permits us to predict the development of a localized necking instability as a function of the severity of the imperfection f , the strain hardening exponent n and the strain rate hardening exponent m . Of these three variables, two are material properties, (n and m), while f is dependent on the non-uniformity of the brittle section of the wall. Note that the limit strain will not be dependent on the strength coefficient K . Once localized necking occurs, the ductility of the cladding is that of the "limit" strain, which is the far-field strain at the onset of necking.

Applying the analysis above to Zircaloy, using $n = 0.01$ and $m = 0.02$ typical of irradiated Zircaloy [10] we found that even a very small imperfection ($f = 0.03$) can trigger a necking instability, which reduces the limit strain to 0.01. For cladding with a wall thickness of 0.6 mm, this means an embrittled rim of only 18 μm would limit the hoop strain at failure to only 1%, even though the material would normally exhibit higher strains at failure. This is shown in Figure 2 where the strain in the necked region is plotted against the strain in the far-field, for various values of the flaw size f , assuming the strain hardening exponent $n = 0.02$ and strain rate hardening exponent $m = 0.02$, typical of irradiated Zircaloy cladding. The limit strains are indicated by arrows in figure 2. The limit strain for $f = 0.03$ in this case is only 0.017. It should be mentioned that not considered in the present analysis is any flow softening due to dislocation channeling induced by irradiation damage, nor any effect of the high strain rates present during an RIA. Both of these effects should accelerate the localized necking development.

Finally, we note that the above analysis assumed plane-strain deformation of the cladding in which there was no axial extension of the cladding. Previous analyses have shown that the onset of localized necking is quite sensitive to deformation path [6]. Specifically, localized necking is inhibited by deformation paths other than plane-strain tension. For example, deformation in uniaxial tension delays localized necking to strains approximately twice those in plane tension. Thus for cladding whose ductility is limited by the localized necking phenomenon, tests of cladding in uniaxial tension will result in erroneously high indications of cladding ductility if it is subjected to plane-strain tension in service. As a result, we strongly believe that the use of ductility data generated from axial tension tests of cladding can be very misleading in predicting the performance of cladding during a RIA event in which the cladding is likely to be subjected to plane-strain tension. We also note that there may be a significant difference in material behavior between failure due to hoop extension and that after axial extension; for a RIA event, hoop extension is the operative failure path.

4. Discussion

The determination of the mode of failure prevalent in high burnup cladding during an RIA is essential to understanding the phenomenon and developing scientific-based licensing regulations that ensure safety in using high burnup fuel. To predict failures during an RIA, it is necessary to consider all possible modes of failure, including those made possible by the damaged state of the cladding after long reactor exposure. Then more realistic fuel failure limits can be developed.

The mode of failure proposed in this paper had been observed previously by Pettersson et al. [11] who conducted burst tests in Zircaloy cladding irradiated to up to $10.8 \times 10^{24} \text{ n.m}^{-2}$ ($E > 1 \text{ MeV}$). The tests revealed that small surface flaws considerably limited the ductility of Zircaloy cladding when loaded under plane strain conditions. This sensitivity to defects was absent in similar tests the same authors conducted in ring specimens, where the loading is uniaxial [12]. The authors concluded that the combination of plane strain loading and the small amount of strain hardening in the irradiated materials led to the high degree of defect sensitivity observed.

In addition, Lee and Adamson [13] have shown that the load-carrying capacity of irradiated Zircaloy cladding is severely degraded under plane-strain loading conditions when compared with axial tension. They also emphasize that the continuum modeling of the deformation behavior of irradiated Zircaloy cladding may be inadequate because of the localized nature of the deformation. As mentioned above, localized deformation induced by dislocation channeling would further reduce the ductility of the irradiated material.

Our analysis and the experimental evidence above indicated a sensitivity of cladding ductility to a combination of small surface flaws, small work hardening exponent, (associated with reactor exposure) and plane-strain deformation path which can severely limit cladding ductility. The flaw size calculated in section 3 can readily appear in irradiated cladding especially after high burnup. One possibility is uneven cladding oxidation. After 45 GWd/ton, oxide thicknesses of 70-100 μm can exist in some types of cladding [14]. In that case a 20% fluctuation in the oxide thickness could trigger the above mentioned instability. Another possibility is that a hydride rim or blister forms, possibly aided by oxide spalling. The fracture resistance of this rim would be highly dependent on the local hydride distribution, but a hydrided rim of about 50 μm seen in the HBO-1 experiment [5] may well be sufficient to cause cladding failure by localized necking. In contrast, the 25 μm hydride rim in HBO-3 may be an insufficient imperfection to cause failure prior to the 1.5% hoop extension during an RIA.

5. Summary

The failure of Zircaloy cladding during an RIA event has been examined from the standpoint of the occurrence of a localized necking instability limiting cladding ductility. Ignoring the influence of high strain rates (which are also likely to reduce ductility), our

analysis suggests that high burn-up cladding may be susceptible to pronounced losses of ductility under a combination of plane-strain deformation and the presence of thickness "imperfections", such as may be caused by a rim of hydrided material or non-uniform oxidation of the cladding. Applied to Zircaloy cladding with deformation behavior expected of high burn-up fuel, the analysis suggests that a 18 μm flaw, possibly originating from cracking within a thin rim of heavily hydrided material, could cause failure of the cladding at hoop strain levels of less than 1% strain after flaw initiation. The features and general characteristics of this failure mode appear to correspond well the cladding failure observed in the NSSR tests, but not with the CABRI REP Na-1 failure.

Acknowledgments

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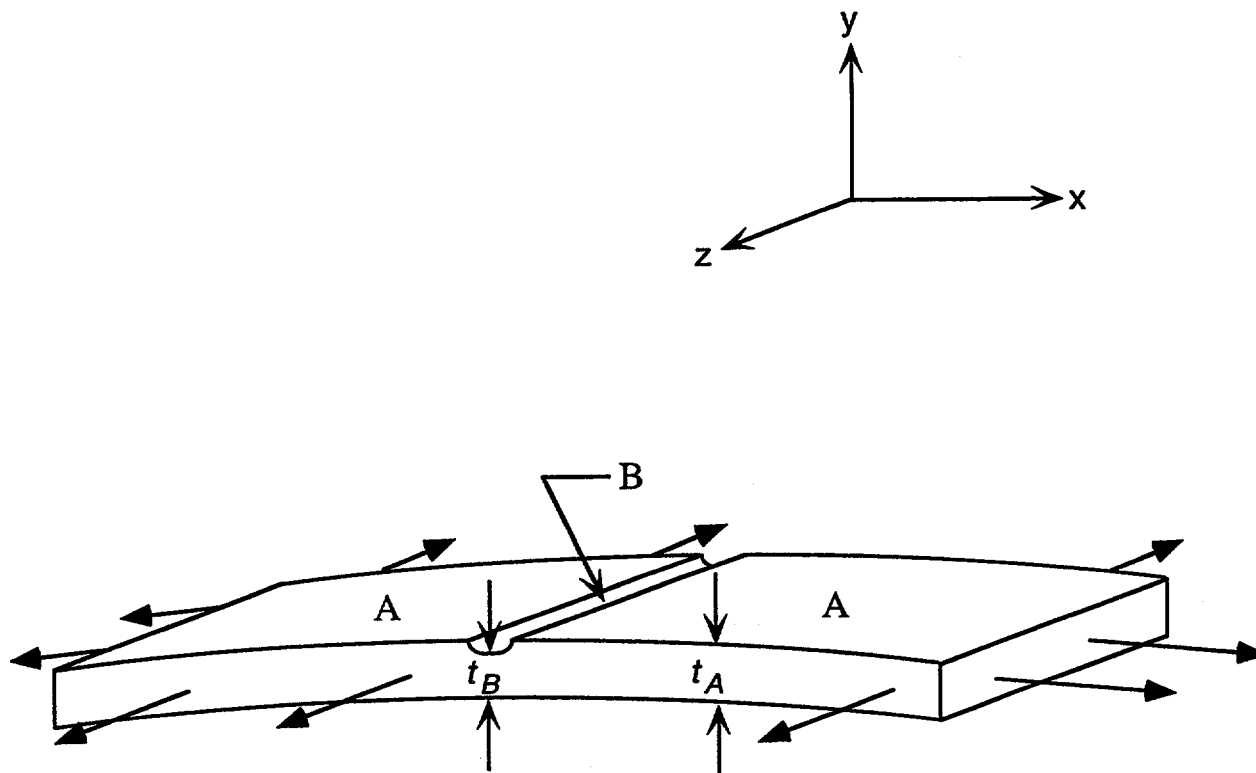


Figure 1: A schematic drawing indicating an imperfection reducing the thickness of the sheet from t_A to t_B . Plane-strain conditions are indicated during sheet extension.

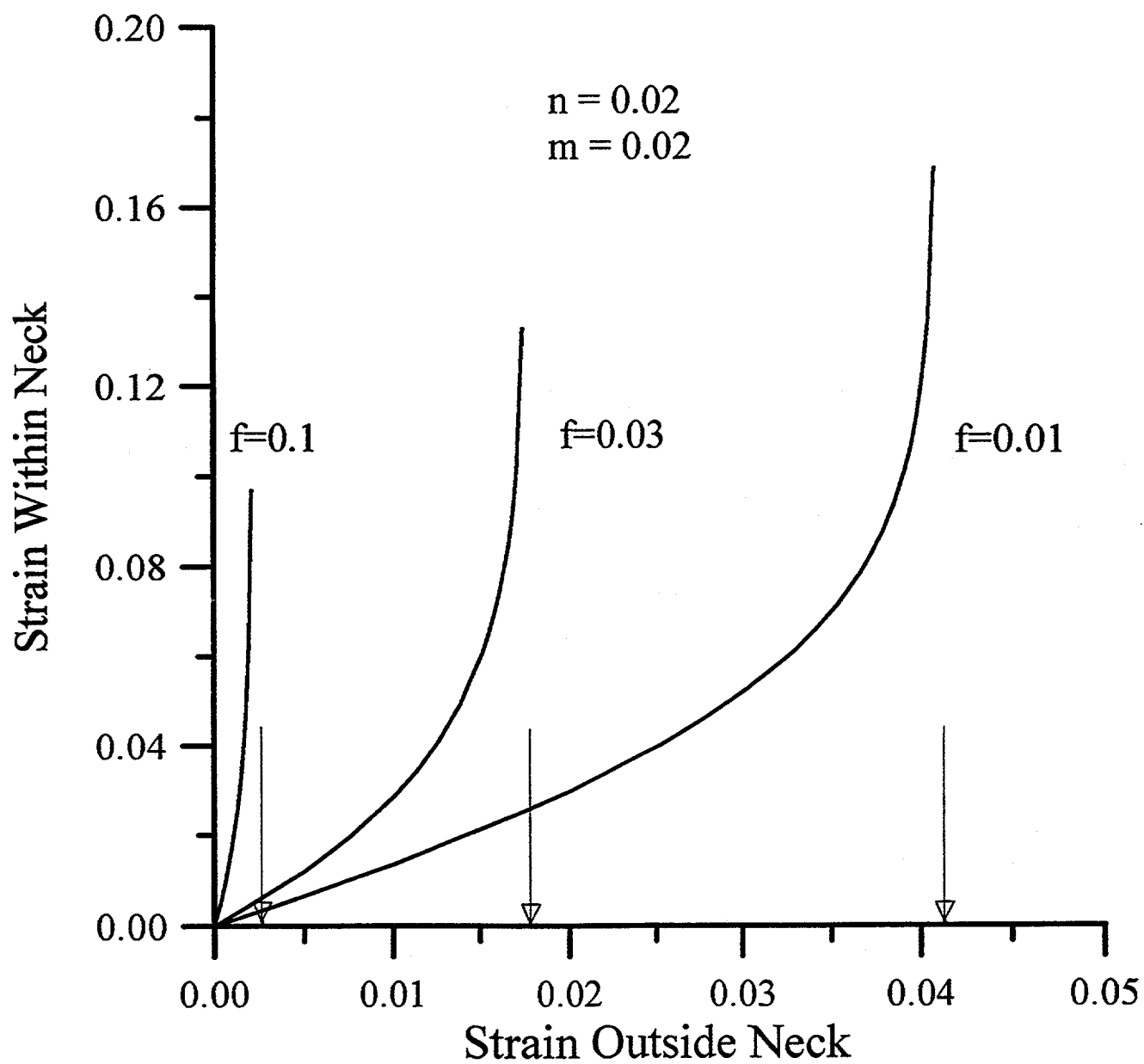


Figure 2. The development of a localized necking instability for the plane-strain deformation of cladding containing imperfections of differing severities f . Strains are maximum principal strain values and arrows denote the limit strain for each case.

1. The first part of the paper is devoted to a general discussion of the problem of the existence of solutions of the system of equations

2. The second part of the paper is devoted to a detailed analysis of the case of the existence of solutions of the system of equations

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FRENCH INVESTIGATIONS OF HIGH BURNUP EFFECT ON LOCA THERMOMECHANICAL BEHAVIOR

Part one

Experimental programmes in support of LOCA design methodologies

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ABSTRACT

Within the framework of Burn-Up extension request, EDF, FRAMATOME, CEA and IPSN have carried out experimental programmes in order to provide the design of fuel rods under LOCA conditions with relevant data.

The design methods used in France for LOCA are based on standard Appendix K methodology updated to take into account some penalties related to the actual conditions of the Nuclear Power Plant. Best-Estimate assessments are used as well.

Experimental programmes concern plastic deformation and burst behavior of advanced claddings (EDGAR) and thermal shock quenching behavior of highly irradiated claddings (TAGCIR).

The former reveals the important role played by the α/β transformation kinetics related to advanced alloys (Niobium alloys) and the latter the significative impact of hydrogen charged during in-reactor corrosion on oxidation kinetics and failure behavior in terms of cooling rates.

1- INTRODUCTION

In the early 1970's, EDF decided to work out the best-estimate calculation code CATHARE to investigate accidental transients like loss-Of-Coolant Accident (LOCA). To support this numerical approach, relevant experimental data were needed. Several experimental programmes were carried out in cooperation with CEA and FRAMATOME.

One of the oldest is related to the mechanical behavior of the cladding under postulated LOCA conditions. It started in 1974, just after the issue of the NUREG 630 recommendations. The objectives of this programme called EDGAR were to verify the actual behavior of standard and new Zircaloy alloy claddings under conservative calculated LOCA transient conditions and secondly to supply experimental data for the development of a deformation model and a burst criterion.

In 1989, EDF asked for a burn-up extension license from 47 GWd/tU to 52 GWd/tU (F/A average). To support this request, an extensive irradiation test programme was undertaken. The objectives were to identify the limiting parameters related to high burn-up and to verify that available requirements and criteria were still applicable for highly irradiated materials.

Among these requirements, those concerning the behavior of the fuel rod under LOCA conditions as well as under Reactivity Initiated Accident (RIA) were particularly emphasized.

In cooperation with IPSN, EDF started by the end of the 1980's a specific study of the thermal shock quenching behavior of highly irradiated Zy4 cladding in order to check the applicability of 10CFR50 requirements.

This analytical experimental programme, called TAGCIR, was completed in 1996. It used irradiated cladding samples retrieved from standard Zy4 rods irradiated up to 64 GWd/tU.

Following a short presentation of EDF LOCA design approach, the main results of EDGAR and TAGCIR tests programmes are given in part one and in part two of this paper respectively.

2- EDF LOCA DESIGN METHODS

Every time that fuel management or fuel rod or fuel assemblies designs change, LOCA design studies have to be performed. Most of them are made according to the standard method based on 10CFR50 Appendix K requirements. Recently Best-Estimate analysis were performed and presented to the French Safety Authorities.

2.1 Standard methods

Within the framework of the new optimized fuel management GARANCE, related to Burn-Up extension in 900 MWe French Nuclear Power Plant, EDF has developed a specific methodology, based on the Appendix K standard method.

This latter has been updated by taking into account the axial offset and the irradiation impact on initial stored energy (radial power profile in the pellets and initial average pellet temperature), on initial oxide thickness, on pellet clad initial gap and on burst behavior of the cladding.

Beside these updatings related to high burn-up fuel management GARANCE, penalties have been taken into account in the LOCA analysis. These penalties are induced by the fuel management itself, the presence of MOX fuel, the percentage of plugged steam generators tubes (or supposed to be plugged in a next future), the presence of various fuel designs in the core, the burn-up extension and the extended reduced power operating.

Of course, the available design margins are not enough to allow to account for all these penalties together. In addition, all Nuclear Power Plant (NPP) do not exhibit all penalties simultaneously. In order to take into account the real condition of each NPP, a sensitivity study has been carried out, the impacts of each penalty on the key parameters and LOCA calculations results have been assessed and expressed in term of "bonus" or "malus" and then applied in each case according to the real state of the NPP.

2.2 Best estimate method

To take into account R&D results, 10CFR50 have been revised by US NRC in September 1988. The purpose of this revision was to allow the use of best estimate methods as an alternative of Appendix K.

In these methods, based on second generation computer codes, the quantitative assessment of the uncertainties takes the place of contractual safety margins.

EDF has started to work out, more than 20 years ago, the second generation code CATHARE. This code has been carried out in cooperation with FRAMATOME and IPSN. It is now available for Safety Analysis. In early 1996, licensing was obtained from French Safety Authorities. It concerns long term LOCA analysis and intermediate break case. Both analysis were presented with regard to the new long cycle fuel management of 1300 MWe French NPP. The licensing of the large break case is underway.

The French best estimate methodology remains in the main stream of the Regulatory Guide 1.157 prescriptions [1]. The basic principles of the French approach are the following ones:

- the transient is physically analysed in order to identify the key phenomena and to check the code adequacy. The dominant parameters are of various types : modelling, simplifying assumptions, schematization choice, initial conditions and boundary conditions. This analysis is based on physical knowledge and on experimental results. Sensibility studies can also ground and complete eventually this analysis.
- then the calculation uncertainties are assessed, emphasizing on dominant parameters. This work is based on sensitivity calculations. As an alternative, the conservatism of the code can be eventually checked with regard to the key physical phenomena.

- if necessary, some biases are then introduced to take into account these uncertainties. These biases which are close to the key phenomena can be applied on modelling or on schematization option or on a boundary condition or in a decoupled calculation.
- when elementary uncertainties are well determined, they are combined statistically (on initial or boundary conditions or on code correlations) to assess the calculation uncertainties.

The French approach is deterministic, intermediate between previous Appendix K methodology and more recent Regulatory Guide 1.157 statistical approach [1]. French procedure leads to a more conservative assessment of the calculation uncertainties than required in the Regulatory Guide but remains consistent with its method principles :

- objectives are to provide an estimate of the calculation uncertainty and to demonstrate compliance with safety criteria at a high confidence level ,
- penalization mode is chosen so that no unrealistic results can be predicted by the code.

3- SUPPORT EXPERIMENTAL PROGRAMMES IN FRANCE

In the early 1980's a few in pile LOCA tests have been performed in the French test reactors SILOE (Flash experiments) and PHEBUS (Phebus-loca tests). Today the French strategy to address the need of experimental data for LOCA transient analysis consists of studying separately plastic deformation and thermal shock quenching behavior of the cladding through two specific experimental tests programmes :

- Plastic deformation, ballooning and burst criterion of the cladding during LOCA blowdown are investigated through the analytical tests programme EDGAR (see below).
- Oxidation and thermal shock behavior of highly irradiated are investigated through the tests programme TAGCIR (see part two of this paper).

4- IN PILE TESTS

• *Single rod tests in the SILOE reactor (Grenoble, France)*

Experiments have been carried out in the FLASH loop in the SILOE test reactor, using single 300 mm long rods [2]. The primary aim of the programme is to study fission product release during a LOCA, but data have also been obtained on cladding deformation and oxidation and on fuel stack behavior.

The loop is blown down with residual fission power simulating decay heating, and is then reflooded. Peak strains of 18-62 % have been recorded. These data have been applied to validation of calculation codes.

• *Multi-rod tests in the PHEBUS loop (Cadache, France)*

The CEA has carried out a programme in the PHEBUS loop, which is installed in a pool type reactor, to study the behaviour of 5x5 fuel rod bundles in a simulated PWR large break LOCA [3]. The rods have an active length of 800 mm and are of the 17x17 PWR type.

The first series consisting of eight thermal hydraulic experiments using impressurised rods and one burst test was started in 1980 and completed in 1982. These experiments were carried out with the fresh fuel rod bundle contained within an unheated shroud. In the burst test, 21 of the rods were initially pressurised to 4 MPa to cause them to burst in the high alpha phase region, the remainder did not deform owing to their low internal pressure.

The pressurised rods burst within 14 s of one another at the top of the ramp position of the transient at about 840 °C in steam, just prior to reflooding. The scatter in burst times is considered to have resulted from non uniform rewetting which affected clad deformation by emphasizing circumferential temperature gradients.

The strains in the peripheral rods were appreciably lower, 15-35 %, compared with those in the central 3x2 array, 43-53 %. This is an expected consequence arising from the steep circumferential temperature gradients induced by the unheated shroud.

The average flow restriction for the central 3x3 array was 65 %. However, the greatest individual flow channel reduction was approximately 80 %.

The LOCA tests programme stopped in 1982. Since this date, the PHEBUS facility is used to investigate the behavior of fuel in severe accident conditions, beyond the design criteria. The test reactor will not be available again before 2003 approximately.

5- OUT OF PILE TESTS

5.1 Plastic deformation and burst tests : EDGAR test programme

5.1.1 Objectives

An extensive programme (EDGAR) has been carried out on internally pressurised Zircaloy tube specimens (500 mm long) using direct heating, to investigate the behavior of fresh and irradiated materials (standard Zy 4 and Niobium advanced claddings) under postulated LOCA conditions[4].

The first objective of this programme has been to verify the actual behavior of Zircaloy cladding under conservative calculated large break LOCA two-peak transient conditions such as those considered in PWR standard safety reports. To achieve this target, the test facility (figure 1) is able to simulate the whole LOCA two-peak transients by monitoring internal pressure and cladding temperature evolutions. During the test, internal gas temperature, external cladding temperature (up to 1200 °C), internal gas pressure (up to 200 bar), diametral strains and axial displacement are measured and recorded. Temperatures and strains are measured with accuracies of 1 % and 0,1 % respectively.

The second objective of the EDGAR programme has been to supply experimental data for the development of a deformation model and a burst criterion, which constitute a part of the data basis of calculations codes as CATHARE code. The experimental data used for this modelling are retrieved from more than 200 individual tests either with constant pressure and constant heating rate (0,2 to 200° c/s) or with constant temperature and constant pressure rate (0,01 to 0,2 MPa/sec) or creep tests (figure 2). Some specific tests have been added to take into account the annealing effect of the first thermal spike into the α - β or β phase on the subsequent mechanical behavior of the cladding during the two-peak transient.

The third objective was to study the influence of irradiation on the mechanical behavior of Zircaloy. For that, an EDGAR type rig has been set in a hot cell in Saclay. The first transient tests performed with irradiated cladding have shown a larger creep rate of the irradiated material for the first 10 s of the two-peak transients. Moreover, the ultimate hoop stress of the irradiated cladding decreases at burst in the 800-950° C range from 15-20 % compared with fresh cladding.

The test programme on irradiated material stopped in 1987 owing to technical concerns. The design of a new rig to be set in a hot cell in 1998 is underway. The tests programme on unirradiated cladding is still undergoing and provides relevant data on advanced cladding materials.

5.1.2 EDGAR tests results as a basis for modelling

As opposed to NUREG 630 models depending on empirical correlations EDGAR model relies on a phenomenological description of the thermomechanical behavior of the cladding.

The creep law and the burst criterion take into account the impacts of phases transformations of Zirconium in terms of temperature kinetics, pseudo-primary creep and annealing effects (related to the first thermal peak of the postulated two-peak LOCA transient).

This model does not take into account implicitly the postulated azimuthal gradient in fuel bundles : this has to be taken into account explicitly by using the EDGAR model in a LOCA 2D calculation code. Anyway, axisymmetric temperature distribution overestimates the circumferential strains (and subsequently the blockage) : the data provided directly by EDGAR facility are then conservative.

Without going too much into all the details of EDGAR modelling it can be said that the model consists in 5 secondary creep rate laws determined in terms of temperatures ranges related to α , $\alpha - \beta$ et β phases fractions in the cladding.

That implies to assess the transformations laws as precisely as possible (figure 3). The creep laws are corrected with 2 factors to take into account on the one hand the fact that the temperature is not constant during a postulated LOCA transient and, on the other hand, the fact that the microstructure could change during the first thermal peak of the postulated LOCA two-peak transient (annealing effect).

In addition the decrease of material ductility related to oxygen diffusion (cladding oxidation) is simulated by substituting actual temperature for equivalent temperature.

The model includes a burst criterion which consists of an empirical correlation linking the true stress before ballooning to the cladding rupture temperature and to the α phase fraction (figure 4).

The blockage ratio is calculated using NUREG 630 method and the local strains deduced from EDGAR model.

Figure 5 gives the time to rupture and uniform elongation predicted with EDGAR model.

5.1.3 Comparison NUREG 630 - EDGAR models

Philosophically NUREG 630 and EDGAR are dissimilar :

- NUREG 630 model is a global implicate model whose target is to calculate the **consequences** of the claddings burst on the fuel bundles cooling.
- EDGAR model is a phenomenological model to simulate the thermomechanical **behavior** of the cladding during complex postulated LOCA transient.

In figure 6 the Hardy law (NUREG 630) overestimates the EDGAR creep rates in α phase particularly at low stresses.

Burst criteria are compared in figure 7. NUREG 630 is always more conservative particularly at low stresses.

5.1.4 Specific behavior of Niobium alloys

We have seen that deformations mechanisms of the cladding during a LOCA transient are complex :

- temperature variation is not monotonic (irreversible transformations),
- most of the transient occurs in a temperature range where zirconium alloys transform,

- the transformations from α to β phases do not occur under thermodynamical equilibrium but during sharp temperature ramps.

Niobium alloys are known to exhibit specific β/α transformation kinetics. The transformation temperatures are lower compared with Zy 4. Moreover they change with the temperature ramp rate of the transient. This phenomenon is compensated by a lower transformation kinetic and globally the thermomechanical behavior of Niobium alloys seems to be equivalent to a standard Zy 4 behavior.

Nevertheless, this specific behavior depends upon temperature kinetics, i.e. the postulated LOCA transients shapes. To be used for LOCA analysis, models have to take into account this particularity.

5.1.5 Specific behavior of irradiated/hydrided Zircaloy

In NUREG 630, it is assumed that irradiation has negligible impact on thermomechanical behavior of the cladding during a postulated LOCA transient.

Nevertheless, two experimental observations could be in contradiction with this point :

- the few EDGAR tests carried out on irradiated claddings exhibited higher creep rates compared with unirradiated claddings tested in the same conditions,
- recent analysis of TAGCIR tests (see part two of this paper) has shown that hydrogen, even in small quantities, could have a significative impact on β/α transformation domains and on the kinetics of these transformations. Hydrogen is supposed to lower the $\alpha + \beta/\beta$ limit, to enlarge the $\alpha + \beta$ domain and to lower the $\alpha/\alpha + \beta$ limit.

All these factors incite the experimenters to check the hydrogen impact on plastic deformation by testing pre-hydrided fresh claddings. These tests are scheduled in 1997 in EDGAR facility.

In 1998 a new EDGAR rig will be available and set in a hot cell to carry out tests on irradiated advanced zirconium alloys claddings retrieved from spent fuel.

5.2 Thermal shock quenching cladding behavior - TAGCIR test programme

EDGAR tests programme gives data on cladding behavior during the first phase of LOCA, i.e. the blowdown and the two-peak thermal transient.

To simulate the behaviour of the cladding during the second phase of LOCA transient, i.e. the oxidation in steam conditions, the rewetting and the quenching, the specific tests programme TAGCIR has been carried out in EDF Chinon hot cells jointly with CEA/IPSN. The experiments are described and analysed in part two.

6- SUMMARY AND CONCLUSIONS

To improve calculations code models and cladding behavior assessment under LOCA transients, analytical experiments have been carried out for 20 last years in France.

To facilitate the analysis, phenomena occurring successively during a postulated LOCA transient have been investigated separately in 2 sets of experiments : EDGAR (ballooning and burst) and TAGCIR (thermal shock quenching).

The main trends of the tests results are :

- Advanced alloys like Niobium alloys have a specific behavior under typical α/β transformation kinetics related to the temperature rates.
- More physical models are needed to assess the correct thermomechanical behavior of this advanced alloy under a large range of temperature kinetics.

- Irradiation could have an effect on ballooning because hydrogen charged in the cladding during reactor operation is enough to impact β/α transformation temperatures and kinetics.
- Cooling rate could have an impact on mechanical behavior of the cladding owing to the ductile β phase stabilized by hydrogen.

These results will help utility and suppliers to define as clearly as possible the test matrix which is really needed to license an advanced cladding within the frame of LOCA design issue.

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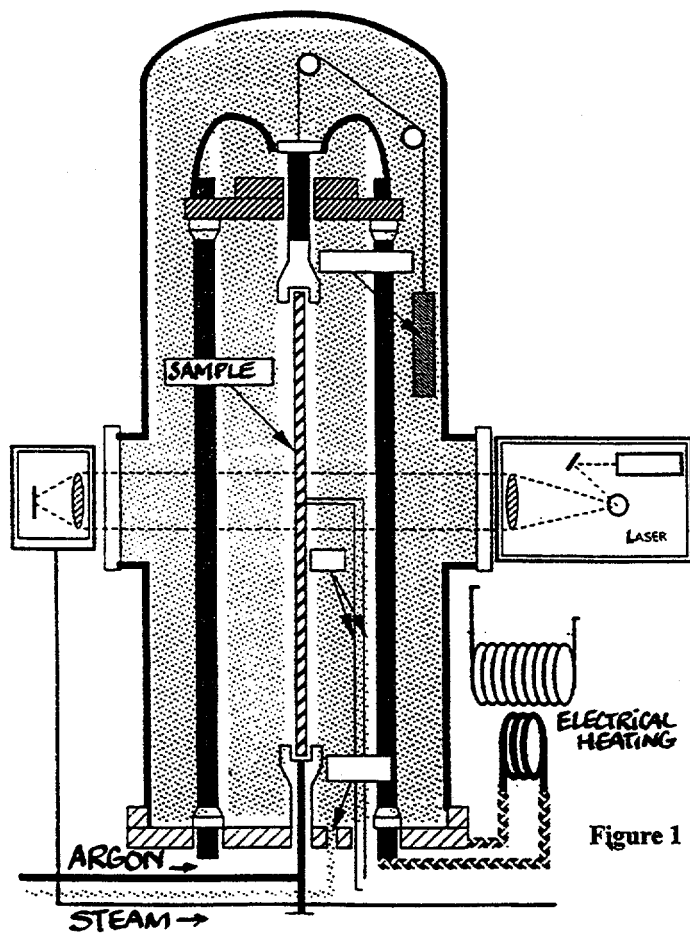


Figure 1 Schematic of the EDGAR tests facility.

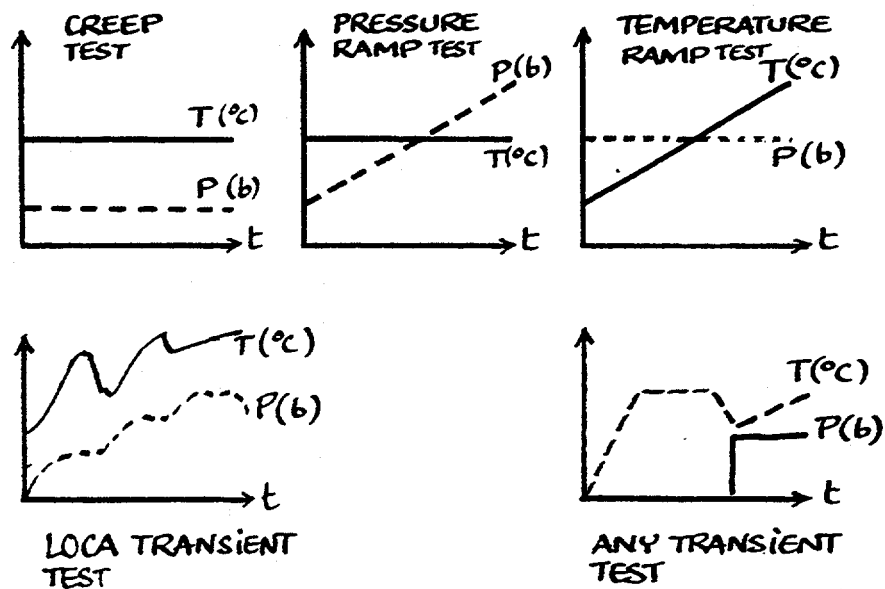


Figure 2 Temperature-Pressure transients tested in EDGAR tests facility.

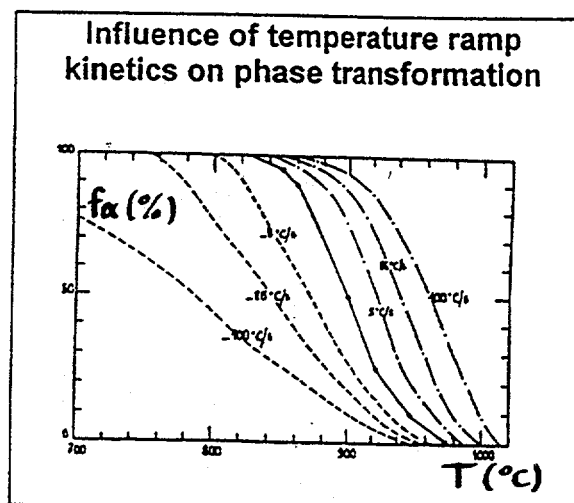
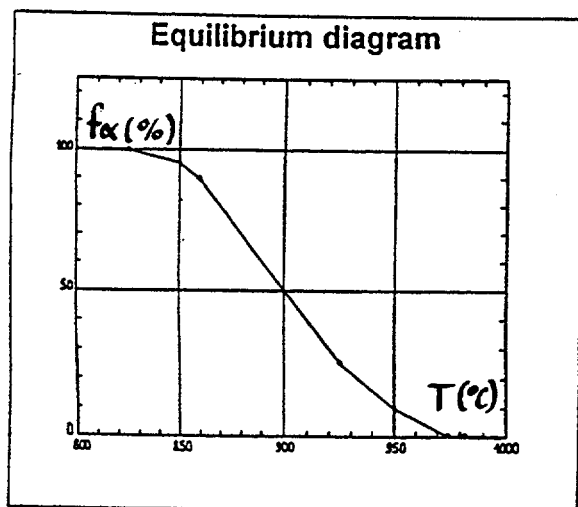


Figure 3 Influence of temperature ramp rate on phase transformation.

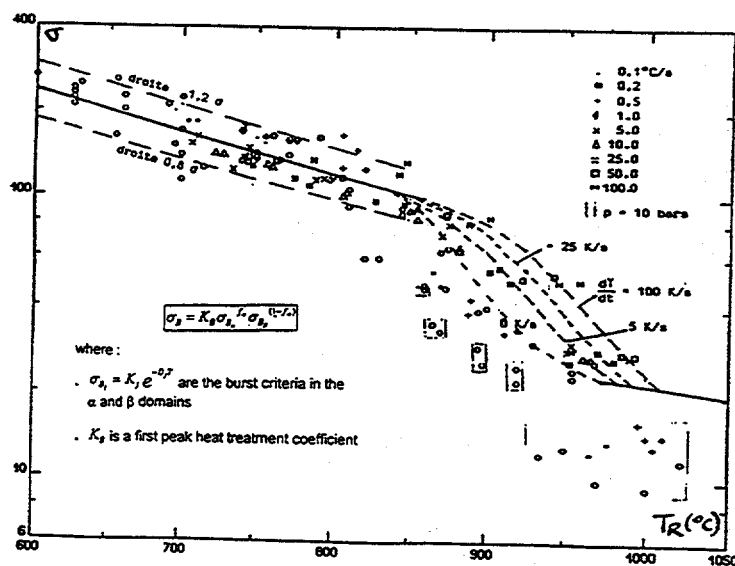
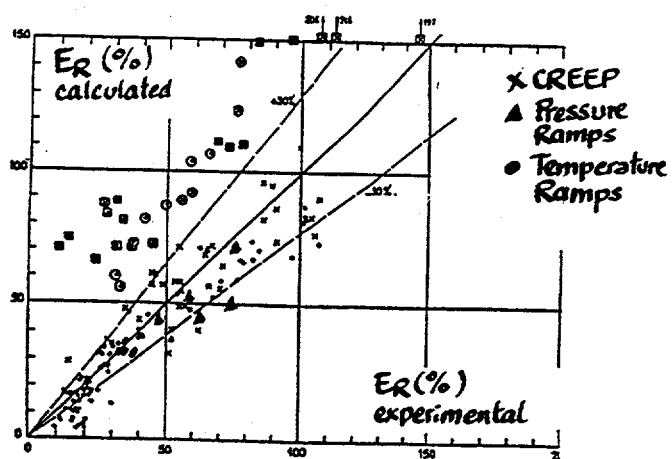
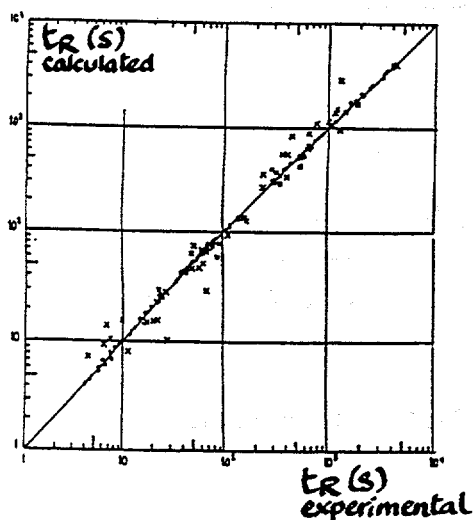


Figure 4 EDGAR burst criterion in term of temperature ramp rate.

Figure 5 Time to rupture and uniform elongation. EDGAR predictions.



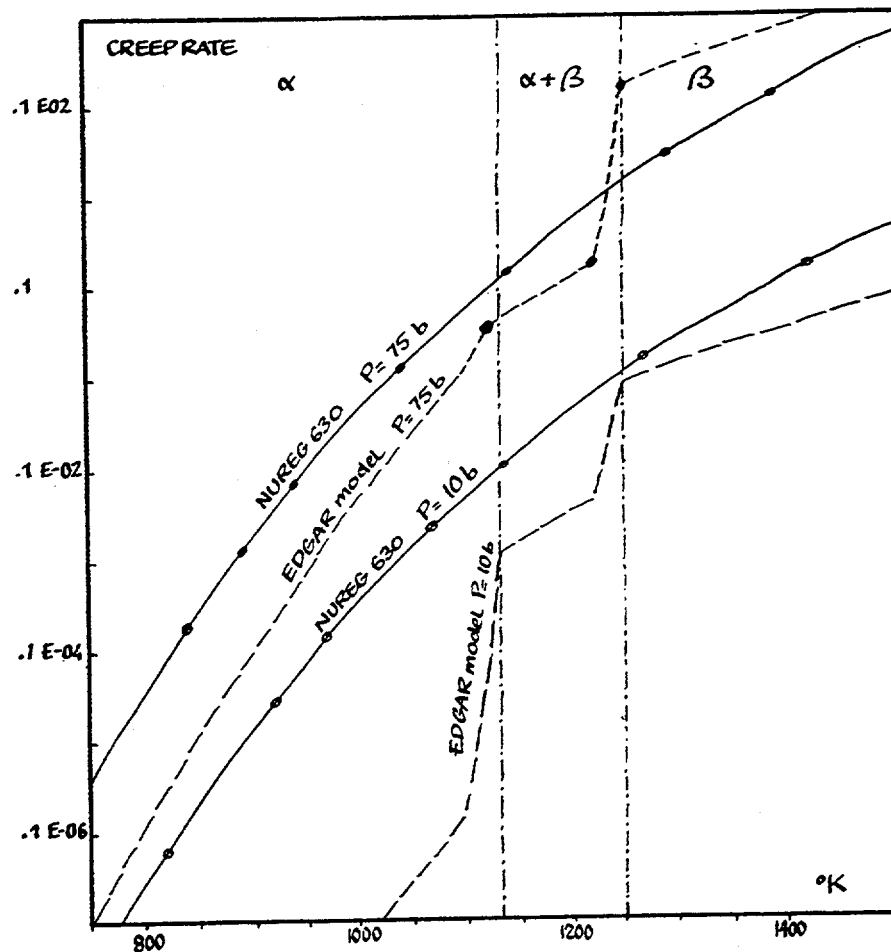


Figure 6 Creep rate versus temperature. NUREG 630 - EDGAR models comparison.

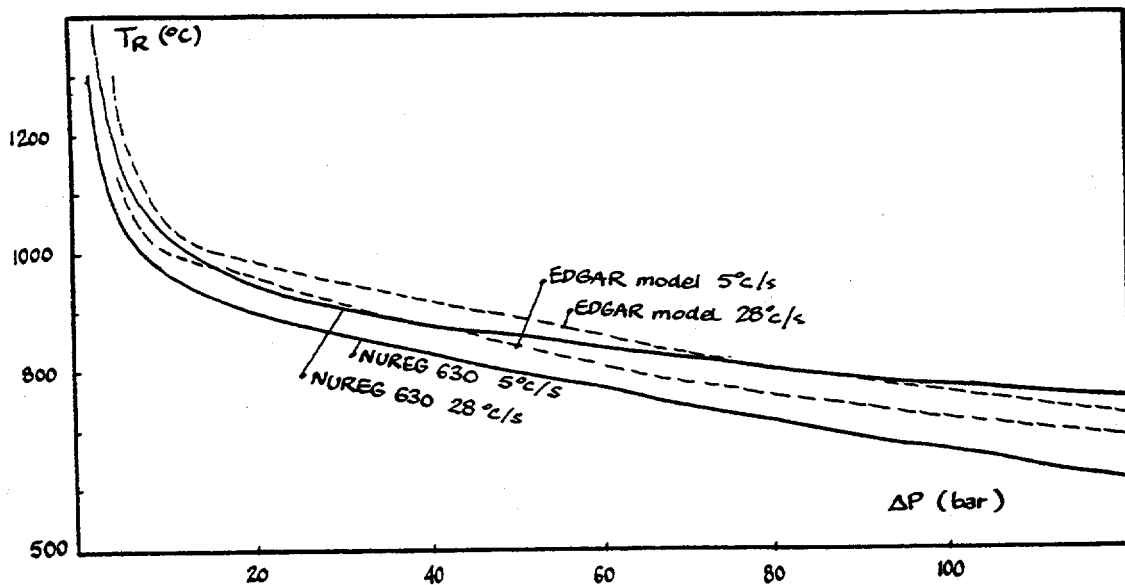


Figure 7 Burst criterion versus temperature ramp rate. NUREG 630 - EDGAR models comparison.

FRENCH INVESTIGATIONS OF HIGH BURNUP EFFECT ON LOCA THERMOMECHANICAL BEHAVIOR

Part Two

Oxidation and quenching experiments under simulated LOCA conditions with high burnup clad material

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ABSTRACT

In the frame of the high burnup fuel studies to support a possible extension of the current discharge burnup limit, experimental programs have been undertaken, jointly by EDF and IPSN in order to study the thermal-shock behaviour of high burnup fuel claddings under typical LOCA conditions.

The TAGCIS program used unirradiated cladding samples, bare or bearing a pre-corrosion state simulating the end-of-life state of high burnup fuel claddings; the TAGCIR program used actually irradiated cladding samples taken from high burnup rods irradiated over 5 cycles in a commercial EDF PWR and having reached a rod burnup close to 60 GWd/tU.

The thermal-shock failure tests consisted in oxidizing the cladding samples under steam flow, on both inner and outer faces or on the outer face alone, and subjecting them to a final water quench. The heating was provided by an inductive furnace the power of which being regulated through monitoring of the sample surface temperature with use of a single-wave optical pyrometer.

Analysis of the irradiated tests (TAGCIR series) evidenced an increased oxidation rate as compared to similar tests on unirradiated samples. Results of the quenching tests series on unirradiated and irradiated samples are plotted under the usual presentation of failure maps relative to the oxidation parameters ECR (equivalent cladding reacted) or e_p (thickness of the remaining beta phase layer) as a function of the oxidation temperature. Comparison of the failure limits for irradiated specimens to those for unirradiated specimens indicates a lower brittleness under two side oxidation and possibly the opposite under one-side oxidation.

The tentative analysis of the oxidation and quenching tests results on irradiated samples reveals the important role played by the hydrogen charged during in-reactor corrosion on the oxidation kinetics and the failure bearing capability of the cladding under LOCA transient conditions. This emphasizes the need for a better understanding of the different physical phenomena where hydrogen can participate.

1. INTRODUCTION

In France the decision was taken by the national utility EDF by the end of the 1980's to generalize annual quarter-core fuel management together with use of 3.7% enriched UO_2 fuel.

To support the authorization of this fuel operation, an irradiation test program has been conducted that allowed to identify the limiting factors related to extended burnup¹. Additionally, under safety considerations, it was requested prior to any generic authorization, that the high burnup fuel behaviour be validated, with the support of appropriate tests results, under Loss-of-Coolant Accident (LOCA) conditions as well as under Reactivity Initiated Accident (RIA) conditions.

In support of the LOCA question, two experimental programs were undertaken jointly by EDF and IPSN in order to study the thermal-shock behaviour under typical LOCA conditions of high burnup fuel claddings, so as to assess the safety margin relative to current acceptance criteria and possibly to update

the acceptable limits on physical parameters characterizing the cladding state during LOCA transients. The TAGCIS program, completed in 1993, used unirradiated cladding samples, bare or bearing a pre-corrosion state simulating the end-of-life state of high burnup fuel claddings ; the TAGCIR program, completed in june 1996, used actually irradiated cladding samples taken from high burnup rods irradiated over 5 cycles in a commercial EDF PWR and having reached a rod burnup close to 60 GWd/tU.

2. EXPERIMENTAL

2.1 Test samples

The TAGCIS tests were performed on different series of unirradiated zircaloy-4 cladding samples :

- as-received standard cladding 0.57 mm thick ("bare" reference samples) ;
- as-received cladding thinned down to 0.525, 0.370 or 0.270 mm ;
- cladding pre-corroded in a pressurized loop with LiOH addition (see details in Ref.[2]) ;
- bare samples 0.525 mm thick, with an initial 500 or 1000 ppm hydrogen content.

Nearly 400 tests runs were performed from mid 91 to mid 93, among which 140 tests results were retained in the final data analysis.

The TAGCIR tests were performed on irradiated cladding samples that were cut from selected high burnup rods irradiated over 5 cycles in the Gravelines EDF reactor ; the average burnup of the selected rods was 60 GWd/tU. Samples were chosen at different axial levels on these rods :

- for about 80 % of the samples, in the medium gridspans (2-3 and 3-4) where oxide scale appears homogeneous with a thickness ranging from 50 to 70 μm ;
- for about 20% of the samples, in the peak gridspan (6-7) where oxide spalling and layering are very important, with a thickness ranging from 60 to 120 μm .

A first series of 30 tests was performed from mid 93 to early 95, among which 25 tests were retained ; a second series of 8 tests was performed in the first half of 1996, the analysis of which is not yet completed.

2.2 Test facility

The thermal transients, simulating LOCA conditions, consisted in oxidizing the cladding samples in steam at an isothermal temperature lying between 1000 and 1300 °C and subjecting the samples to a final water quench.

Figure 1 shows a schematic of the experimental facility used to perform the test series on unirradiated cladding ; a replica of this facility was installed in a hot cell for the irradiated cladding tests. The heating is provided by an inductive furnace the power of which is regulated through monitoring of the sample surface temperature with use of a single-wave optical pyrometer.

During a test the cladding sample is suspended vertically in a quartz tube by mean of a stainless steel inner tube allowing the sample inlet feeding with either steam or argon flow ; this supporting piece is connected to a pneumatic jack allowing the rapid transfer of the sample to the quenching tank, located just below the quartz tube, at the end of the oxidation phase.

The temperature indicated by the optical pyrometers is a brightness temperature which may be significantly lower than the true surface temperature, depending mainly on the surface emissivity and to a lower extent on the transmission factor of the quartz tube. For the TAGCIR tests late series, the specific oxidation tests (CODAZIR) and the current program on pre-hydrided samples (HYDRAZIR) a laser pyrometer has been used additionally, giving a continuous measurement of the surface emissivity, thus providing the true temperature of the sample surface.

2.3 Experimental Procedures

Two main series of tests were performed, with oxidation respectively on the two sides or on the outer side alone of the cladding sample.

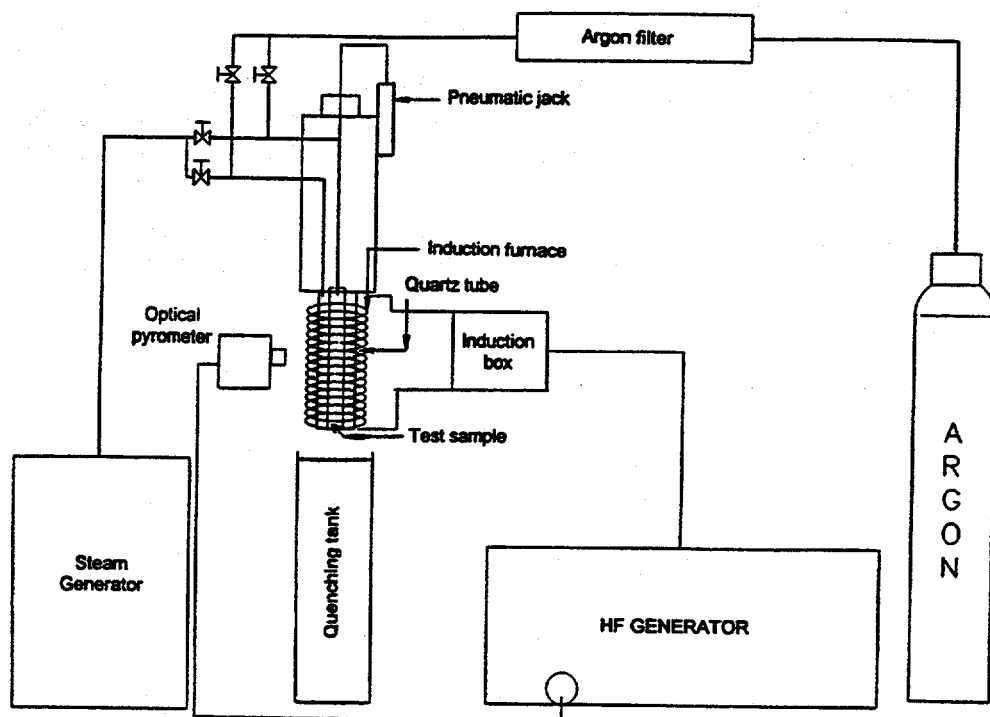


Figure 1. Schematic of the experimental facility

For two-side oxidation tests the samples were 70 or 100 mm long with the bottom end left open and the steam flowing on both inside and outside. For one-side oxidation tests the sample were longer (130 mm) and closed at the bottom end with a plug maintained at low temperature by immersion in the quenching tank ; the sample inside was maintained in argon overpressure relative to the outside so as to prevent steam or air ingress on the inside.

For the temperature measurement of the pre-corroded or irradiated samples, in order to prevent thermal screen effects due to a possible layering of the initial oxide scale, this one was removed by mechanical erosion on a small area corresponding to the area viewed by the optical pyrometer ; this experimental feature makes similar the effective temperature transients at the oxidation front for bare and corroded samples tests.

Very few tests samples appeared fragmented or bore visible important cracks after quench ; most of them remained intact or looking like. To further investigate the embrittlement level of these samples they were subjected to a gastightness check by connecting the two endcaps to a nitrogen or argon overpressure and dipping the sample into water ; observation of gas bubbles indicates a path of connected cracks through the wall thickness, corresponding to an important brittleness of the sample : accordingly, all the samples that revealed non gastight during the check were declared failed. Handling experience of these samples showed that, although apparently almost intact, they were indeed very brittle and could fragment upon minimum loads.

After testing, samples were sectioned in the vicinity of the pyrometric measurement location for metallographic examinations under optical microscopy ; the widths of oxide, α and prior β layers were determined. Segments of selected samples were also analyzed for hydrogen content, after testing, by an inert gas evaporation technique.

The method for determining the failure after quench as well as the experimental quenching procedure are supposed to play an important role in the level of conservatism of the results.

3. RESULTS

3.1 Unirradiated tests

3.1.1 Analytical procedures

Several models of increasing complexity were tested for the prediction of the oxidation final state before quench : different parabolic rate laws (Cathcart, Urbanic,...) and a diffusion model derived from the PECLOX code³. The predictions of these models were compared to measured values of oxide and alpha layers thicknesses from various tests on unirradiated cladding samples. As an example, figure 2 gives a plot of the predictions from the diffusion model versus measured values of the outer oxide layer thickness for the tests on bare cladding with various initial thickness. The agreement between measurements and model predictions appears here quite fair. It remains also satisfactory for the tests on pre-corroded samples, so that the initial oxide may be considered as fully transparent (non protective) for the oxidation process developing underneath during LOCA transient.

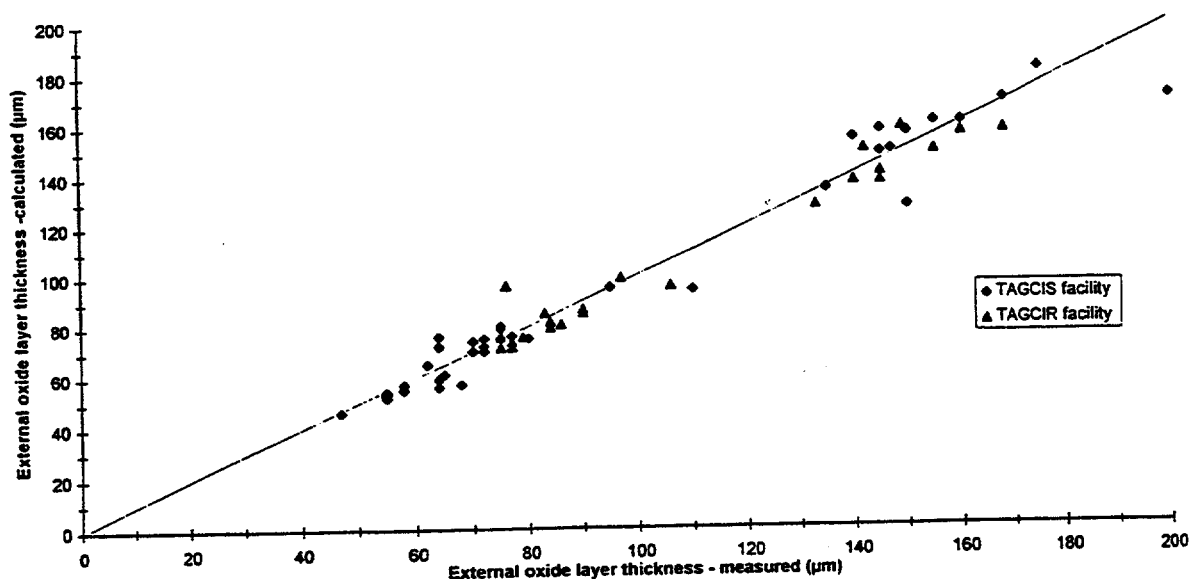


Figure 2. Calculated versus measured outer oxide thickness for unirradiated bare samples tests

However we must point out that these model predictions were issued from a computation using the brightness temperature indicated by the single-wave pyrometer as the oxidation temperature. We will discuss later in this paper of the dramatic consequence of taking into account of the surface emissivity and associated temperature correction.

Taking consideration of the fair predictive performance of the diffusion model and in view of possibly defining failure limits relative to parameters non directly available from metallographic measurements (such as the β -Zr thickness with an oxygen content less than a threshold value, as proposed by Chung and Kassner⁴) the analysis of all the unirradiated tests of the TAGCIS series was conducted with use of the parameters values as calculated by the diffusion model.

3.1.2 Results

The failure bearing capability of an oxidized cladding under LOCA quench loads is usually referenced to parameters describing the final oxidation state before quench. Three main oxidation parameters have been used for the correlation of the failure results of the quenching tests :

e_{β} : thickness of remaining β phase layer

F_{β} : fraction of remaining β phase

ECR: equivalent cladding reacted = fraction of the initial zircaloy that would be converted to oxide if all the oxygen absorbed by and reacted with the cladding were converted to stoichiometric ZrO_2

While the e_{β} parameter is defined unambiguously whatever initial state of the cladding, for a pre-corroded or irradiated cladding the ECR parameter may refer either to the transient oxidation alone or to the cumulated initial and transient oxidations and, along with F_{β} , may be relative either to the metal thickness before transient or to the thickness of a fresh cladding ; it has been discussed in a previous paper [2] that the initial and transient oxidations should not be simply summed up in an ECR parameter for the two processes are physically different and contribute differently to the embrittlement of the cladding ; accordingly, the following results relative to ECR parameter for pre-corroded or irradiated cladding will be expressed for transient oxidation alone, the initial oxidation being taken into account only for the associated reduction of zircaloy metal thickness available for transient oxidation.

The comparison between bare versus pre-corroded samples is illustrated on figure 3 which gives, for the two-side oxidation tests, a failure map relative to the thickness e_{β} of the remaining β phase layer as a function of oxidation temperature. This map shows a slightly higher limit for pre-corroded than for bare cladding ; however the difference remains within the uncertainty on the initial thickness of metal, resulting from the oxide scale thickness estimate from eddy current measurements, so that the shift on the failure limit for pre-corroded cladding, as shown on figure 3, is probably not representative of an actual embrittlement increase associated to the initial corrosion of the cladding.

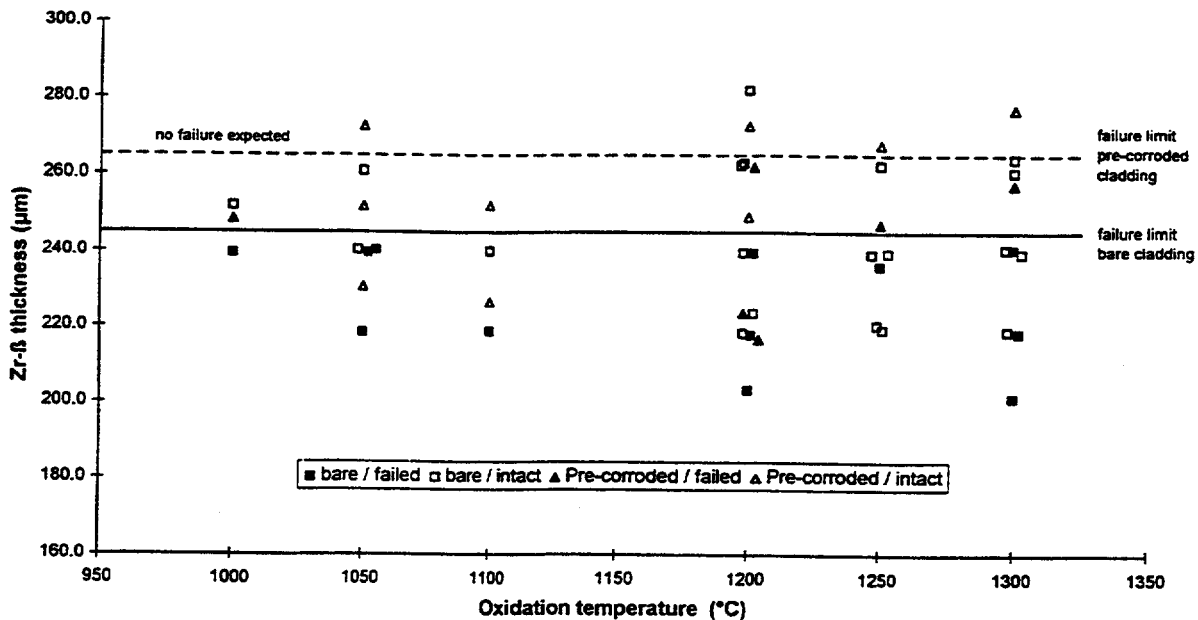


Figure 3. Unirradiated tests. Failure map relative to the β -Zr remaining thickness. Two-side oxidation

Figure 4 gives a failure map of the pre-corroded specimens, oxidized on one or two sides, relative to the ECR parameter as a function of oxidation temperature. The figure shows a slightly higher limit for two-side oxidation (ECR ~ 21%) than for one-side oxidation tests (ECR ~ 20%) ; the difference is however not very significant and a limit at 20% ECR may conservatively be chosen as the ECR limit for all unirradiated samples tests.

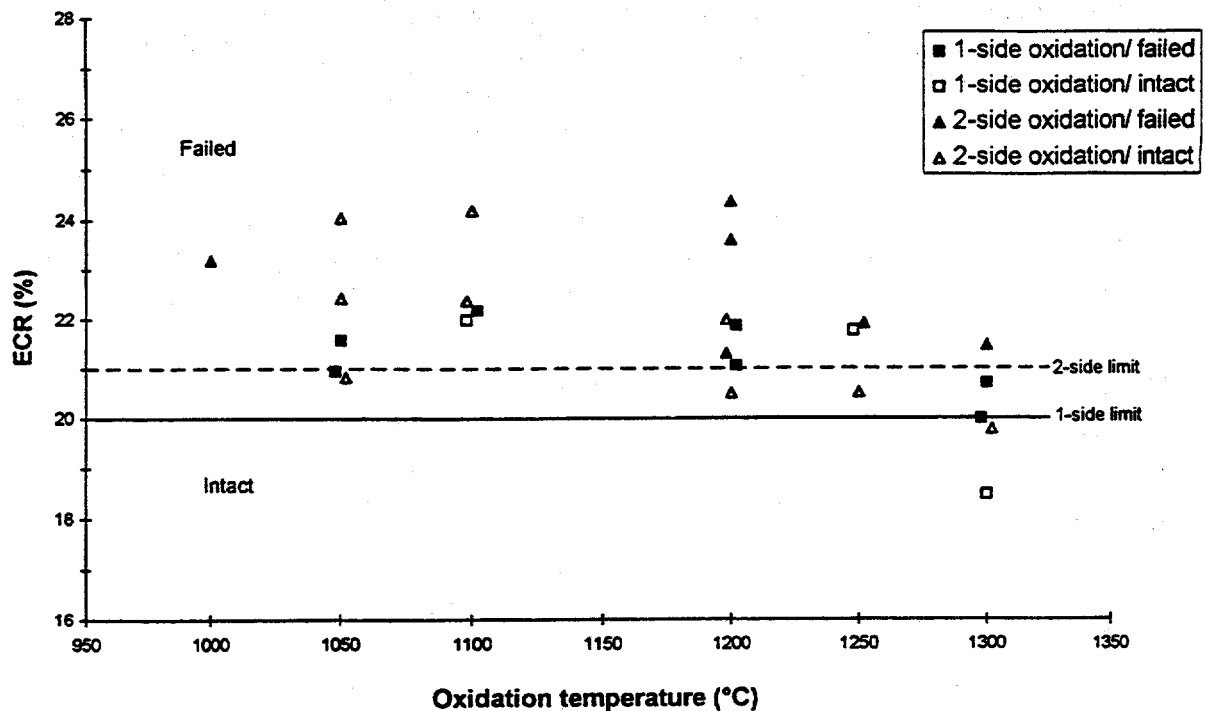


Figure 4. Unirradiated tests. Failure map relative to ECR. Pre-corroded samples.

3.2 Irradiated tests

3.2.1 Analytical procedures

The analysis of the irradiated tests results was initiated with the same tool as for unirradiated tests, i.e. the diffusion model, and with the corresponding assumption that the initial oxide scale is fully transparent to oxygen diffusion under transient temperature conditions. But, surprisingly, unlike the good agreement for unirradiated tests as shown on figure 2, a systematic model underprediction from 20 to 40% was observed on the first four irradiated tests. An experimental deviation of the temperature measurement was immediately suspected, but had to be ruled out by the results of new validation tests on as-received cladding that led again to the expected agreement. An "irradiation" effect on the oxidation kinetics was then suspected and further demonstrated in several scoping twin tests, one on as-received cladding and the other on irradiated cladding where the initial oxide scale had been stripped of by mechanical erosion over the experimental length of interest. These tests indicated an increase in oxide growth ranging from 13 to 30% ; an increase was also noticed for alpha layer growth, but the large scatter in the azimuthal measurements does not allow to report here a reliable value.

As a detailed study of the oxidation kinetics of irradiated zircaloy in the LOCA temperature domain was not the objective of the TAGCIR program, a additional program with specific oxidation experiments (CODAZIR tests) was defined and scheduled for the second half of 1995.

Since a detailed calculation with the diffusion model used previously was no more efficient for the prediction of oxide and alpha layers thicknesses, the analysis of the irradiated tests of the TAGCIR series was simply conducted with use of the parameters values derived either directly from the metallographic measurements or via rate laws empirically adjusted, test by test, on these measurements.

3.2.2 Results

Figure 5 gives the failure map of the irradiated specimens, oxidized on one or two sides, relative to the ECR parameter as a function of oxidation temperature. Figure 6 gives the corresponding failure map relative to the remaining thickness of β -Zr, where obviously the failure and intact domains are in reverse

positions^a. Failure limits (ore more exactly non-failure limits) have been drawn as the lower bounds of the values for failed tests. On these figures are also drawn, for comparison, the corresponding failure limits for unirradiated samples tests.

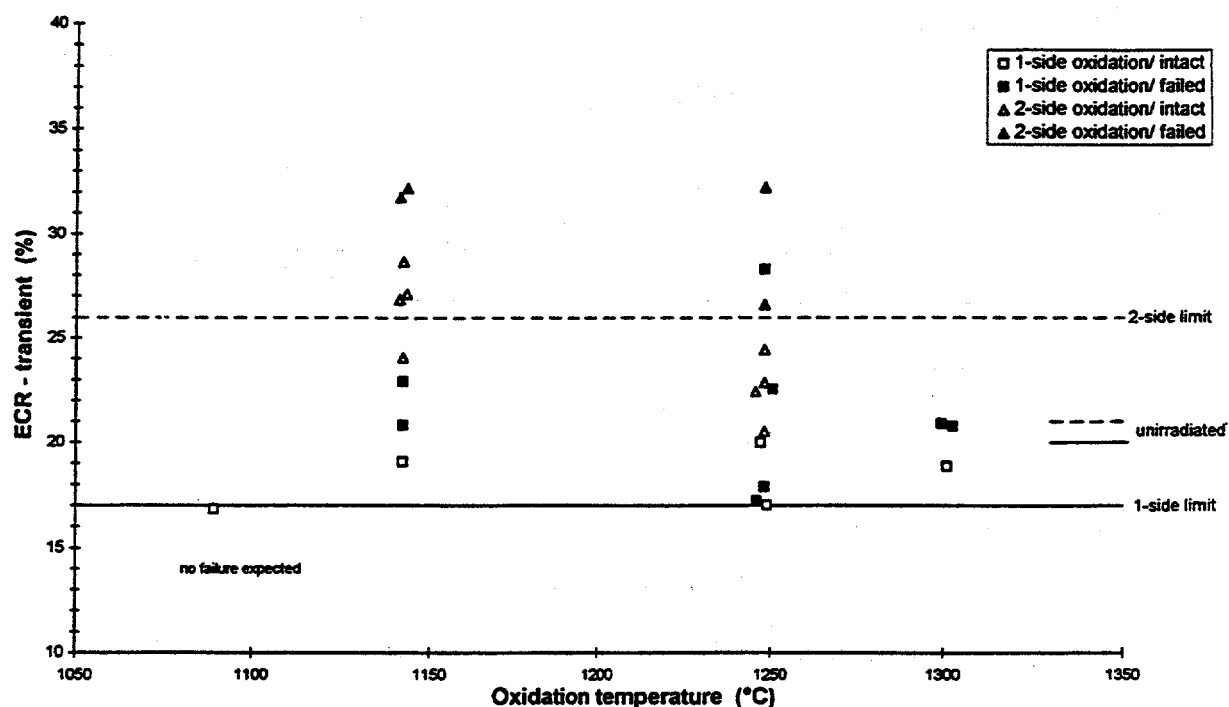


Figure 5. Irradiated tests. Failure map relative to ECR (transient oxidation)

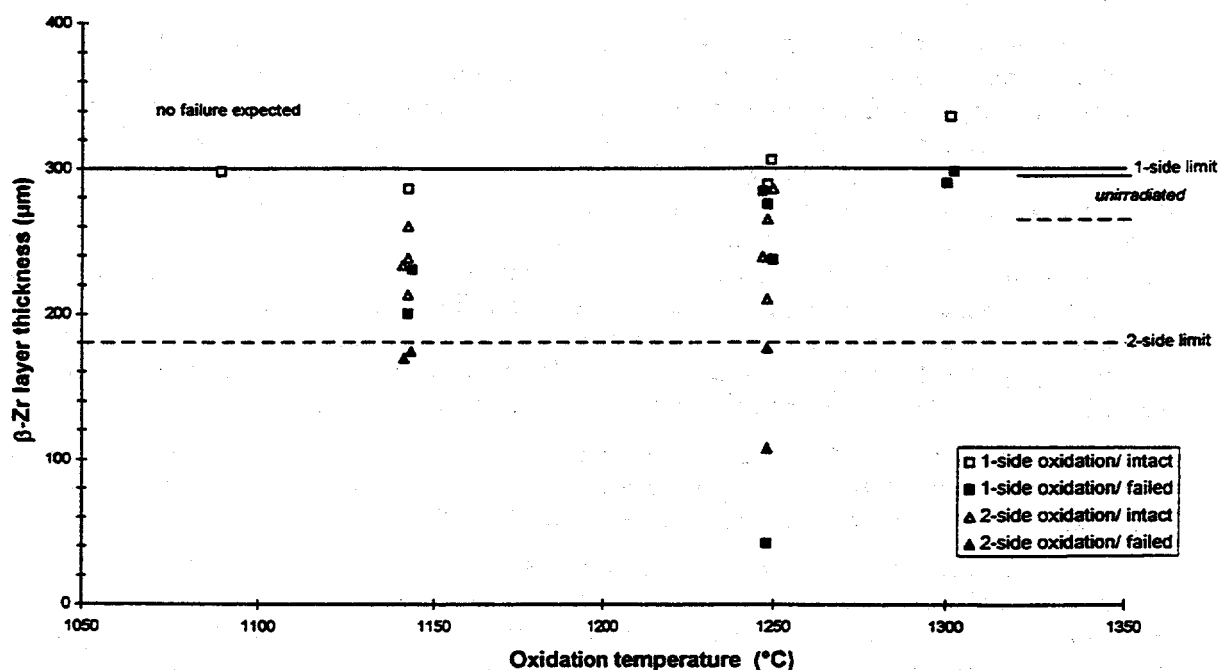


Figure 6. Irradiated tests. Failure map relative to the β -Zr remaining thickness

^a These failure maps have been updated since the previous analysis reported in Ref. 2, mainly to take into account, for one-side oxidation tests, of the residual inner side corrosion and, in a few cases, of a residual low rate alpha layer growth on inner side during the transient.

The ECR failure map (figure 5) shows a significantly higher limit for two-side oxidation (ECR ~ 26%) than for one-side oxidation tests (ECR ~ 17%) ; the unirradiated limits appear located in middle position within the corresponding interval for irradiated results, thus indicating clearly a higher brittleness for one-side oxidation but the opposite for two-side oxidation. The failure limit under one-side oxidation is 17%, i.e. just the value of the current acceptance criteria. No failure was observed with an ECR value below this acceptance limit ; however there are too few data points located below this value to ensure that it makes the actual failure limit. It must also be pointed out that the ECR values reported here are for transient oxidation alone ; adding the contribution from initial corrosion evidently shifts the failure limits to significantly higher values, 23% and 33% respectively for one and two-side oxidation.

The β -Zr thickness failure map (figure 6) still shows an important difference between one and two-side oxidation failure limits (300 and 180 μm respectively) but the limit under one-side oxidation is now almost the same as for unirradiated tests results. From a physical point of view we think that this parameter should be preferred to ECR ones because it includes unambiguously the contributions of initial corrosion and transient oxidation for either one or two-side oxidation cases.

4. DISCUSSION

4.1 Oxidation kinetics : the CODAZIR tests contribution

In order to study in more detail the increase in the oxidation kinetics as early observed in the TAGCIR tests, a specific test program was undertaken with use of the same facility (CODAZIR tests). The tests consisted in isothermal oxidation runs without quench on short cladding rings that were initially stripped of the corrosion oxide by mechanical erosion. For a refined measurement of the oxidation temperature a laser pyrometer was installed, allowing a continuous measurement of the surface spectral emissivity, thus providing the true temperature of the sample surface during the oxidation transient.

Preliminary tests on as-received cladding samples have indicated unexpected low values of the surface emissivity, within a 0.3 to 0.45 interval. Since these values were lower than expected by a factor of 2 or more, an important set of validation studies was realized, with different independent methods. These studies have confirmed the validity of the earlier measurements.

In a following step, a series of oxidation runs were performed in which the temperature was raised at 50 K/s up to different target values, then maintained for 470 seconds before switching off the inductive heating. The various following samples were used :

- as-received,
- pre-hydrided, by cathodic charging, around 500 ppm H content,
- irradiated, with stripping of the initial oxide,

for which the emissivity measurements were found in the ranges indicated below:

Material	As-received	Irradiated + str.	Pre-hydrided
Emissivity range	0.3 - 0.45	0.5 - 0.65	0.7 - 0.85

On figures 7A and 7B are plotted the weight gain as a function respectively of the surface brightness temperature or of the true surface temperature according to the emissivity correction. Relative to the brightness temperature (figure 7A) there appears no distinct behavior between as-received and hydrogen pre-charged specimens ; relative to the corrected temperature (figure 7B) on the other hand, the pre-hydrided specimens are located distinctly above the as-received ones, the irradiated data points lying in between. This suggests that :

- a) the oxidation kinetics of zircaloy is widely influenced by its hydrogen content ; this may explain the accelerated oxidation kinetics observed in all TAGCIR tests ;
- b) the relatively low kinetics increase observed for irradiated CODAZIR samples is probably the consequence of the initial preparation of these samples : the mechanical erosion for stripping of

the oxide layer having eliminated a more or less wide sublayer of metal with a high concentration of the hydrides accumulated during in-reactor operation.

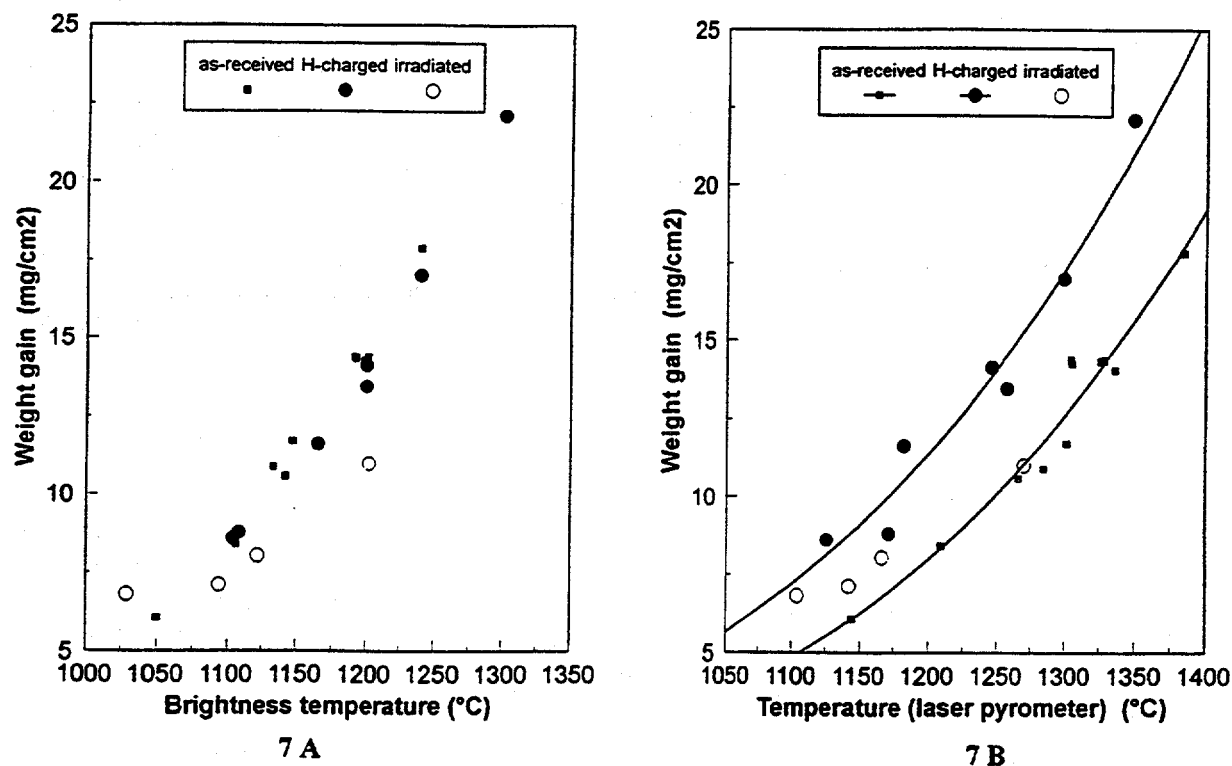


Figure 7. CODAZIR tests results (470 seconds at temperature)

For the reason b) the already prepared CODAZIR samples were considered no more representative of an high BU cladding and the program was interrupted. However, further with remark a), it is thought that the increase in oxidation kinetics for irradiated samples is mainly due to hydrogen content and could therefore be quantitatively studied in oxidation tests on unirradiated but pre-hydrided samples. This is part of the objectives of the HYDRAZIR test program currently in progress.

The temperature correction associated to the surface emissivity has a significant impact on the oxidation kinetics and its prediction by the models. The lower is the emissivity, the larger is the difference between the true and brightness temperatures : for example, at 1100°C brightness temperature, $\Delta T = 26^\circ\text{C}$ for $\epsilon = 0.8$, while it reaches 112°C for $\epsilon = 0.4$. If the emissivity measurement provided by the laser pyrometer is reliable _as seems to be confirmed by the validation studies_ it results that the oxidation temperatures are significantly under-estimated for unirradiated samples tests ; consequently, the fair agreement between measurements and model predictions as shown on Figure 2 would be quite misleading and would just indicate an overestimation by the model roughly compensating the temperature underestimate. For irradiated samples tests, the higher emissivity associated with hydrogen content results in a lower temperature correction, thus making the increase in oxidation kinetics more important relative to the true temperature than relative to the apparent temperature. However, relative to the true temperature, the Baker-Just model appears still slightly conservative for irradiated tests. If the emissivity measurement and associated temperature correction were to be invalidated by new analysis, the temperature underestimation and model overestimation would be reduced, but still present ; in that situation however, the prediction of the oxidation rate for irradiated tests with use of the Baker-Just model would appear non conservative in most of the cases.

4.2 Embrittlement behavior

The relative behavior under one-/two-side oxidation, nor the relative behavior irradiated/unirradiated, are presently not fully understood.

For the unirradiated tests the failure limits under one and two-side oxidation are not very different (Figure 4) and may likely be explained in terms of constraints distribution at the thermal shock of the quench acting either on outer side alone or on both inner and outer sides. It must also be pointed out that the axial temperature distribution is somewhat different in the two configurations, due to the bottom end plugged and cooled for one-side oxidation tests, thus introducing a sharp temperature gradient near the bottom end. Consideration of this last feature makes the one-side oxidation tests results somewhat questionable, and possibly low representative in respect of the actual in-reactor situation.

For the irradiated tests, the one-/two-side differential behavior is widely emphasized, along with a significantly lower brittleness than for unirradiated tests under two-side oxidation (figures 5 and 6). A pure irradiation effect can hardly be suspected due to the annihilation of irradiation defects that is supposed to occur during the high temperature transient. A chemical effect, associated with corrosion, seems to be the most likely factor that may explain the differential behavior between irradiated and unirradiated tests.

In light of the main result of the CODAZIR tests concerning the effect of hydrogen on the oxidation kinetics of zircaloy, it is thought that hydrogen could also play an important part in the failure bearing capability of an irradiated zircaloy cladding. Our proposed understanding is tentative and very preliminary. It is based on the following elements :

- observation of a low rate of α inclusions in the β -Zr region, as revealed by metallographic examinations of some specific oxidation tests on pre-hydrated specimens ;
- consideration of the Zr-H binary diagram, indicating (see figure 8) the possible presence of the β phase down to the eutectoid temperature (550°C) when hydrogen content is high enough (a few hundred ppm), allowing to leave the α domain to enter the $\alpha+\beta$ domain.

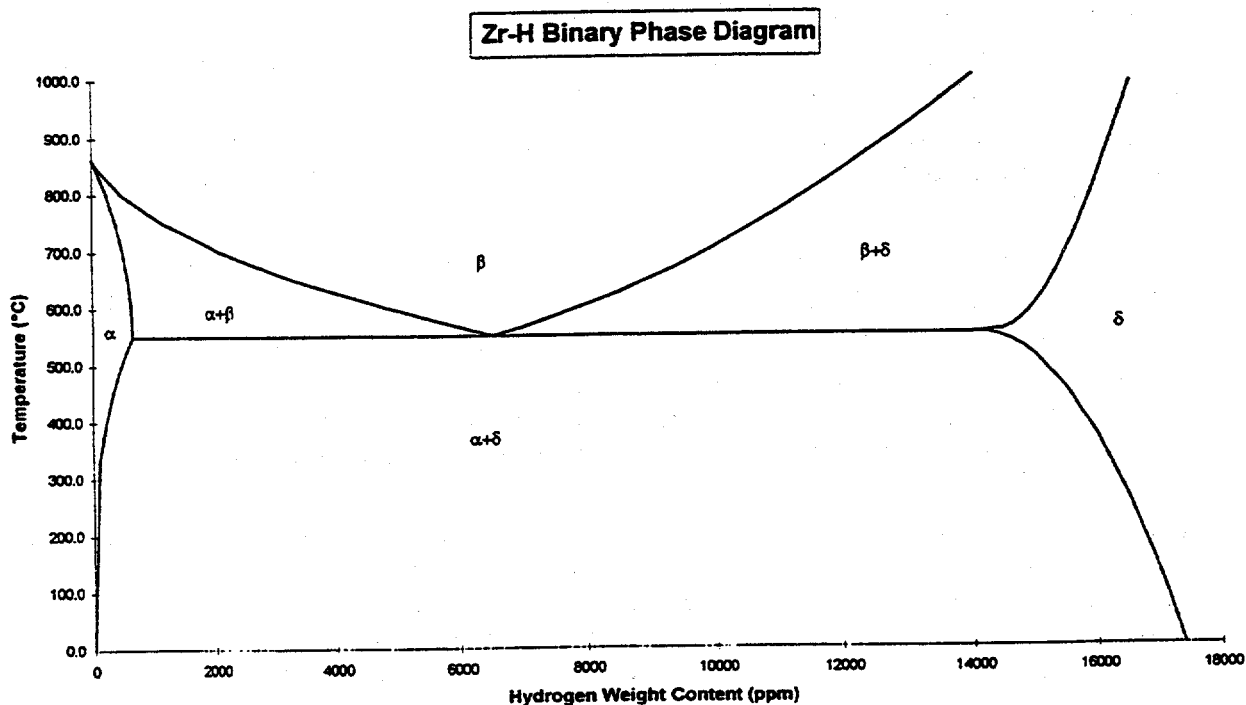


Figure 8. Zr-H Binary Phase Diagram

These elements suggest that the hydrogen charged in the zircaloy cladding during in-reactor operation may act in favouring the ductile β phase to the detriment of the brittle α -Zr[O] phase, thus improving the cladding resistance under thermal shock loads. This would explain the shift in the failure limits observed for two-side oxidation irradiated tests. The behavior under one-side oxidation is more complex and probably involves, in addition to the previous favourable effect, an adverse effect related to axial temperature gradient and constraints field.

5. FUTURE WORK

The HYDRAZIR program, currently in progress in France, with oxidation kinetics / quenching tests on unirradiated but hydrogen pre-charged zircaloy samples is aimed to provide specific data to improve the understanding of the different phenomena where hydrogen participates.

The initial objectives were, for irradiated cladding, to quantify the effect on failure bearing capability under LOCA thermal shock of the hydrogen absorbed during transient on the inner face of a ruptured cladding, under stagnant steam conditions (as evidenced from past JAERI tests⁵). In light of the CODAZIR tests results they have been extended so as to :

- 1) quantify the effect of absorbed hydrogen on the kinetics of the Zr/steam reaction (resumption of previous CODAZIR program)
- 2) quantify the effect of absorbed hydrogen on the failure bearing capability of a zircaloy cladding under LOCA thermal shock, with hydrogen content ranging from in-reactor corrosion values to values typical of inner side transient oxidation.

The program will consist of :

- oxidation tests series on pre-hydrided specimens so as to determine the time/temperature conditions to reach a given oxidation rate for various initial hydrogen charging ;
- quenching tests similar to TAGCIR tests: two-side oxidation under unlimited steam terminated by quench.

6. SUMMARY AND CONCLUSIONS

Thermal transient tests simulating LOCA conditions have been performed on unirradiated bare and pre-corroded cladding as well as on high burnup fuel cladding. Failure limits under quench loads have been investigated. The main tendencies of the tests results indicate :

1. The initial oxide scale appears very poorly protective relatively to high temperature oxidation and may be considered fully transparent to oxygen diffusion.
2. Failure limits for unirradiated pre-corroded cladding are not very different from those for as-received cladding of equal metal thickness, so that initial corrosion does not seem to induce additional brittleness of the cladding.
3. A best-estimate value $ECR=20\%$ is a lower bound for all observed failures on unirradiated tests.
4. Irradiated zircaloy exhibits an increased oxidation rate as compared to unirradiated zircaloy. This seems to be due to the hydrogen charged in the cladding during in-reactor operation.
5. Tests on irradiated cladding have not indicated any failure below the acceptance limit $ECR=17\%$ relative to the transient oxidation alone, although the lowest observed failure is very close to this limit and that few data points were investigated below. Adding the contribution of the initial corrosion shifts the limit to $ECR=23\%$.
6. The failure map on ECR indicates a higher brittleness for irradiated cladding than for unirradiated cladding under one-side oxidation and the opposite under two-side oxidation ; the failure map on the thickness of the remaining β -Zr layer only shows the lower brittleness under two-side oxidation. Hydrogen charged in the cladding during in-reactor corrosion could explain the favourable result through a stabilizing effect of the ductile β phase to the detriment of the brittle α -Zr[O] phase.

The tentative analysis of the oxidation and quenching tests results on irradiated samples reveals the important role played by the hydrogen charged during in-reactor corrosion on the failure bearing capability of the cladding under LOCA transient conditions. This emphasizes the need for a better understanding of the different physical phenomena where hydrogen can participate. The HYDRAZIR program, currently in progress in France, with oxidation kinetics / quenching tests on unirradiated but hydrogen pre-charged zircaloy samples is aimed to provide specific data in this area and could possibly stimulate concerted investigations or further studies that will substantiate the basic knowledge on irradiated cladding behaviour under LOCA conditions.

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TEST PLAN FOR HIGH-BURNUP FUEL BEHAVIOR UNDER LOSS-OF-COOLANT ACCIDENT CONDITIONS*

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ABSTRACT

Excessive oxidation, hydriding, and extensive irradiation damage occur in high-burnup fuel cladding, and as result, mechanical properties of high-burnup fuels are degraded significantly. This may influence the current fuel cladding failure limits for loss-of-coolant-accident (LOCA) situations, which are based on fuel cladding behavior for zero burnup. To avoid cladding fragmentation and fuel dispersal during a LOCA, 10 CFR 50.46 requires that peak cladding temperature shall not exceed 1204°C (2200°F) and that total oxidation of the fuel cladding nowhere exceeds 0.17 times total cladding thickness before oxidation. Because of the concern, a new experimental program to investigate high-burnup fuel cladding behavior under LOCA situations has been initiated under the sponsorship of the U.S. Nuclear Regulatory Commission. A hot-cell test plan to investigate single-rod behavior under simulated LOCA conditions is described in this paper. In the meantime, industry fuel design and operating conditions are expected to undergo further changes as more advanced cladding materials are developed. Under these circumstances, mechanical properties of high-burnup fuel cladding require further investigation so that results from studies on LOCA, reactivity-initiated-accident (RIA), operational transient, and power-ramping situations can be extrapolated to modified or advanced cladding materials and altered irradiation conditions without repeating major integral experiments in test reactors. To provide the applicable data base and mechanistic understanding, tests will be conducted to determine dynamic and static fracture toughnesses and tensile properties. Background and rationale for selecting the specific mechanical properties tests are also described.

INTRODUCTION

Because of major advantages in fuel cost, reactor operation, and less radioactive waste generated, the overwhelming trend in the nuclear industry is to extend burnup of light water reactor (LWR) fuels. While high-burnup operation offers significant advantages, it tends to push Zircaloy fuel cladding close to its performance limit. Because of this concern, performance of high-burnup fuel under reactivity-initiated-accident (RIA) situations is being investigated extensively. The validity of the cladding embrittlement criteria obtained for unirradiated fresh cladding has been also under question for high-burnup fuels under loss-of-coolant accident (LOCA) situations, a design-basis accident which is higher in probability and more important in consequence than a RIA.¹⁻⁶ At present, to avoid fuel rod fragmentation during reflood quenching and subsequent fuel dispersal, 10 CFR 50.46 requires that peak cladding temperature shall not exceed 1204°C (2200°F) and that total oxidation of the cladding nowhere exceeds 0.17 times total cladding thickness before oxidation.¹

*Work supported by the Office of Nuclear Regulatory Research, U.S. Nuclear Regulatory Commission.

At high burnup, in addition to extensive irradiation damage, Zircaloy cladding is susceptible to excessive oxidation and hydriding, usually in the form of uniform breakaway corrosion in PWRs and nodular corrosion in BWRs. For some type of Zircaloy-4 cladding, maximum oxide layer thickness during normal operation to a burnup of ≈ 60 GWd/t in a PWR appears to be close to or exceeds ≈ 100 μm , leaving little margin for additional oxidation during a LOCA to remain below the 17% oxidation limit.

In the meantime, it is not clear whether high-temperature oxidation kinetics of high-burnup cladding applicable to LOCA situations are similar to those of unirradiated cladding. Although data base on high-temperature oxidation kinetics of high-burnup cladding would be required in the evaluation model of emergency-core-cooling-system (ECCS) acceptance criteria³ for high burnup operation, only very limited results have been reported in the literature.⁴ Surprisingly, the initial results reported in Ref. 4 indicate that high-temperature oxidation kinetics of high-burnup cladding may be faster than the counterpart kinetics applicable to unirradiated or low-burnup cladding. Although further investigation is necessary to confirm the initial finding, higher oxidation rates at high temperatures cannot be ruled out considering the characteristic microstructure of high-burnup cladding, i.e., high porosity of the thick breakaway oxide layer and extensive hydriding of the Zircaloy metal underneath the oxide.

High-burnup fuel cladding is also characterized by significant degradation of ductility and fracture toughness, which is attributed to formation of dense hardening centers in the α -phase Zircaloy. The hardening centers are usually associated with excessive oxidation, hydriding, and extensive irradiation damage that are unique to high-burnup operation. Under some conditions, brittle-type failures are produced as a result. Some of the hardening centers (e.g., impurity-irradiation damage complexes) produced in the high-burnup cladding may not be fully annealed out during the high-temperature excursion in a LOCA situation, and as a result, cladding failure by subsequent reflood thermal shock could be influenced adversely. Because of these concerns, a new experimental program to investigate high-burnup fuel cladding behavior under LOCA situations has been initiated under the sponsorship of the U.S. Nuclear Regulatory Commission. The primary objective of this paper is to identify and describe hot-cell test plans that are most suitable to determine the high-burnup fuel behavior under LOCA situations.

In the meantime, industry fuel design and operating (irradiation) conditions are expected to undergo further changes as more advanced cladding materials are developed. Under these circumstances, mechanical properties of high-burnup fuel cladding require further investigation so that results from LOCA and RIA studies can be extrapolated to modified or advanced cladding materials and altered irradiation conditions without repeating major integral experiments in test reactors. Of particular importance are the effects of alloy chemistry, fast neutron fluence, oxidation, hydriding, test temperature, ductile-brittle transition phenomenon, and strain rate on ductility and fracture toughness applicable to LOCA, RIA, power ramping, and other transient situations. At present, the data base and mechanistic understanding in these areas are very limited. To provide applicable data base and mechanistic understanding, parallel tests will be also conducted in this program to determine mechanical properties of several types of high-burnup cladding. Hot-cell test plans and rationale for selecting the specific mechanical properties tests and microstructural analyses are also described in this paper.

LOCA SIMULATION - BASIC CONSIDERATIONS

The focus of this experimental program is to provide (1) a sufficient data base, (2) good correlation of test results with post-test microstructural analyses, and (3) mechanistic understanding applicable to high-burnup fuel under LOCA situations. Specifically, the following questions will be addressed:

- (A) Is the 17% oxidation limit valid at high burnup?

- (B) What are the key metallurgical and thermal-hydraulic factors that influence fuel failure (during LOCA) at high burnup?
- (C) What is the effect of extensive hydriding in high-burnup cladding?
- (D) What are the criteria that can predict best the high-burnup cladding integrity under conditions of (1) reflood quenching thermal shock, (2) post-LOCA handling, and (3) combined seismic load and thermal shock?
- (E) Are such criteria applicable regardless of the type of cladding material (e.g., standard Zircaloy, low-tin Zircaloy, Zirlo, and other non-Zircaloy materials), cladding geometry (initial thickness, degree of ballooning and rupture), and irradiation condition (temperature, burnup)?
- (F) What are the high-temperature oxidation kinetics that can predict best the oxidation behavior of high-burnup cladding under LOCA situations?

To understand high-burnup effects, it is useful to review the background of the current 17% oxidation limit⁵ and evaluate significance of other alternative embrittlement criteria developed for unirradiated fresh cladding.² Essentially, the 17% oxidation criterion was established on the basis of the thermal-shock data reported by Hesson et al. in the early 1970s.⁵ The data were obtained on relatively thick unirradiated Zircaloy-2 BWR cladding specimens (thickness $\approx 1,000$ μm) in which oxide layer thickness was negligible prior to exposure to simulated LOCA transients. After oxidation at $>1300^\circ\text{C}$, thermal-shock tests were conducted by quenching the heavily oxidized specimens directly in cold water. The isothermal oxidation temperatures were, however, considerably higher than the current peak temperature limit of 1204°C (2200°F).

Compared to the simulated LOCA conditions of Hesson et al., high-burnup PWR fuel cladding under LOCA situations is believed to exhibit several important variations in thermal-hydraulic and metallurgical parameters. These are illustrated schematically in Figs. 1 and 2, respectively. To begin with, thickness of modern PWR cladding is only $550\text{--}625$ μm . And, as indicated in the time-temperature curve in Fig. 1, ballooning and rupture occur in a high-burnup cladding during the heatup phase. Therefore, cladding wall thickness will be reduced significantly in the rupture region. This effect, compounded by the effects of smaller initial wall thickness and thicker oxide layer from high-burnup operation, tends to produce significantly thinner load-bearing β -phase in cladding at the time of rewetting (Fig. 2). More realistic heatup rates applicable to high-burnup fuel will be also evaluated and simulated in the LOCA test.

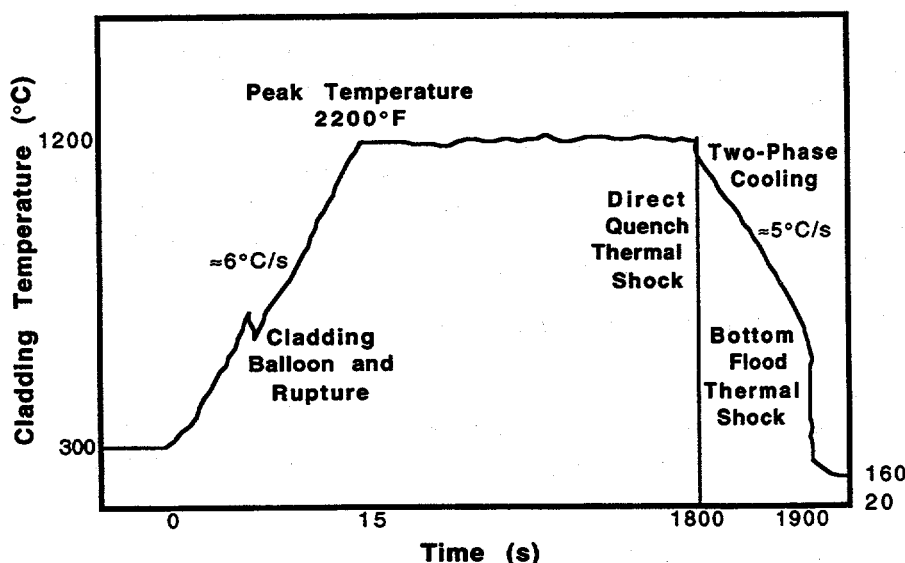


Figure 1.

Illustration of cladding temperature vs. time and quenching thermal shock under a simulated LOCA condition

Another important factor is the relatively slower two-phase cooling. The slow cooling in the mixture of steam and water vapor precedes the thermal shock which is produced when the film boiling collapses and rewetting of the hot oxide surface occurs. The rewetting temperature, measured on heavily oxidized unirradiated Zircaloy-4 specimens, has been reported to be lower than $\approx 650^{\circ}\text{C}$.² Therefore, the magnitude of the thermal-shock stress will be influenced significantly by the mode of quenching, as well as by cladding thickness.

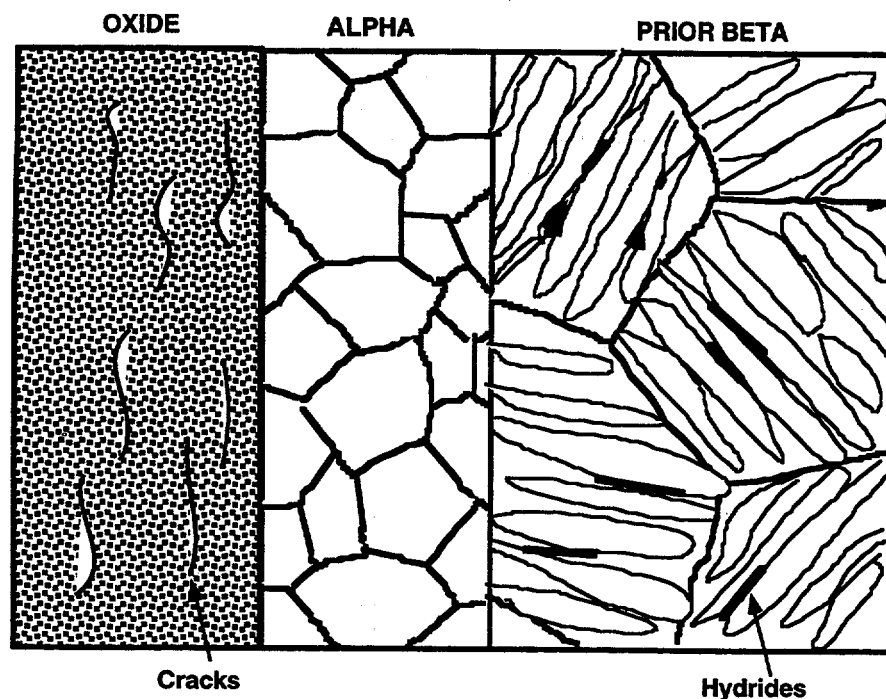


Figure 2.

Schematic illustration of microstructure of high-burnup Zircaloy-4 cladding expected at time of thermal shock during reflood quenching under LOCA situations.

The Widmannstaetten grain structure and the morphology and distribution of hydrides in the loading-bearing transformed- β -phase layer are also strongly influenced by cooling rate of the β -phase at $>815^{\circ}\text{C}$ (Fig. 2). Under direct quenching, hydride precipitation in the transformed β phase is minimal. Hydrogen uptake and hydride precipitation are believed to play an important role in the failure behavior of high-burnup cladding; therefore, both types of simulated LOCA tests will be conducted, i.e., direct fast quenching and thermal shock at $\leq 700^{\circ}\text{C}$ following the slower controlled cooling (Fig. 1).

Effects of fine microstructural features of the oxide- and α -phase layers and the morphology and distribution of hydrides in the α -phase cladding are believed to be only secondary in influencing the failure behavior during reflood quenching. However, a thick oxide layer such as that depicted in Fig. 2 will significantly reduce thermal conduction across the cladding wall at the time of quenching. This will produce an insulating effect which is conducive to smaller temperature differential across the thickness of the transformed- β -phase layer, thereby reducing the magnitude of thermal shock under otherwise similar conditions.

The thermal-shock-failure data reported by Hesson et al.⁵ are shown in Fig. 3, together with similar data reported from direct fast quenching experiments conducted on unirradiated Zircaloys.^{2,4,6} Results in the figure shows that the 17% oxidation criterion is not consistent with the peak temperature limit of 1204°C (2200°F). That is, the 17% oxidation criterion is significantly conservative at $<1204^{\circ}\text{C}$, whereas it is reasonably accurate for peak oxidation temperatures

>1300°C. In view of this, most LOCA thermal-shock tests on high-burnup fuel cladding will be conducted in this program for peak oxidation temperatures <1300°C.

The equivalent cladding reacted (ECR) parameter is a relative oxidation parameter. Therefore, accuracy of a failure criterion based on ECR is subject to variations in not only peak oxidation temperature but also cladding thickness (i.e., variations due to initial thickness, ballooning, and rupture). Consequently, several failure criteria based on more fundamental metallurgical parameters were developed in a previous investigation.² An example of such criteria is that based on the absolute thickness of the load-bearing transformed β -phase of the cladding. In Fig. 4, the ECR data reported in Refs. 2 and 4 and plotted in Fig. 3 have been converted to thickness of transformed β -phase. As shown in the figure, the correlation based on transformed- β -phase thickness appears to be more accurate than that based on ECR. Furthermore, such a criterion is independent of initial cladding thickness. It would be essential to test validity of this type of correlation for high-burnup cladding. Higher levels of oxygen, hydrogen, hydriding, and residual irradiation-induced microstructural features are conducive to lower fracture toughness of transformed β -phase in high-burnup cladding, therefore, a thermal-shock criterion based on transformed- β -phase layer could be influenced significantly.

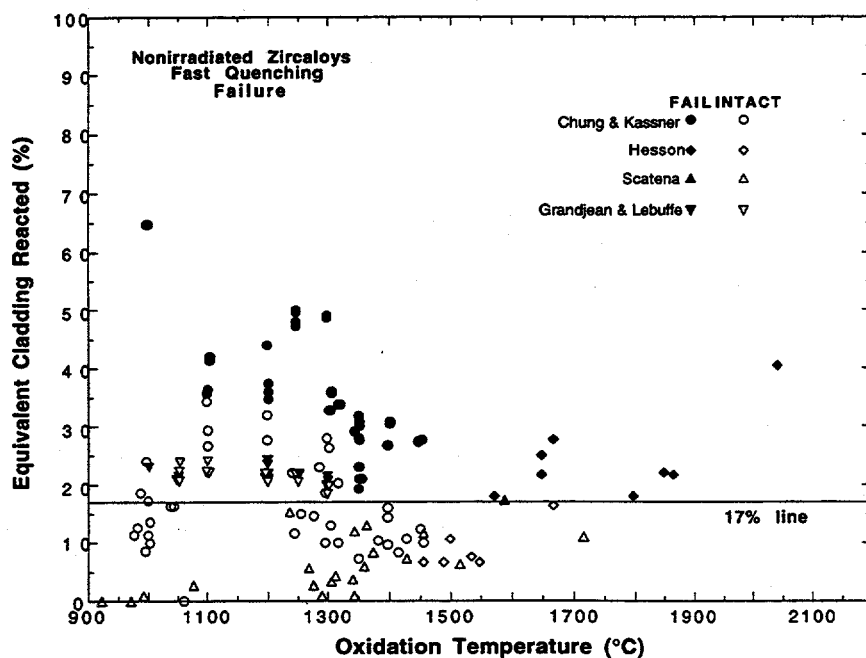


Figure 3.

Thermal-shock failure behavior of unirradiated Zircalloys relative to equivalent cladding reacted. 17% oxidation limit is conservative for <1204°C but more accurate at >1350°C.

More advanced failure criteria based on combined parameters of β -phase thickness and critical oxygen content in the transformed β -phase have been also developed in Ref. 2. Different critical oxygen concentrations in the transformed β -phase were found to correlate well with cladding failure behavior under different cooling and loading conditions. For example, for slow-cooled transformation and subsequent thermal shock at <700°C, a criterion based on >0.1-mm-thick transformed- β -phase layer containing <1.0 wt.% oxygen provided the best correlation. For the condition of direct quenching without slow cooling, a criterion based on >0.15-mm-thick transformed- β phase layer containing <0.9 wt.% oxygen provided the best correlation. Failure under post-LOCA handling (i.e., 0.3-J impact loading) could be best predicted by the criterion based on >0.3-mm-thick transformed- β -phase layer containing < 0.7 wt.% oxygen.

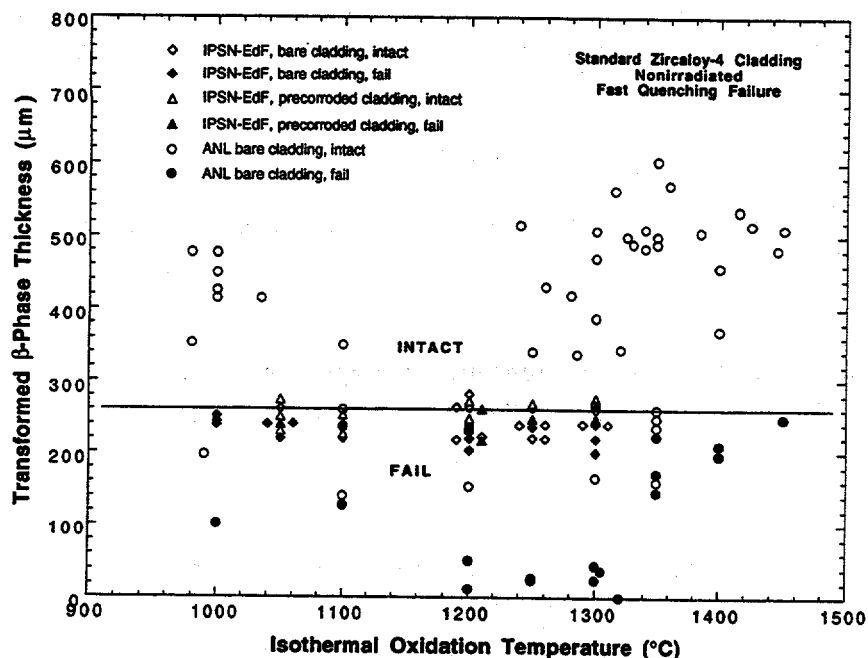


Figure 4.

Thermal-shock failure behavior of unirradiated Zircalloys relative to thickness of transformed β -phase cladding; this correlation is more accurate than that based on ECR.

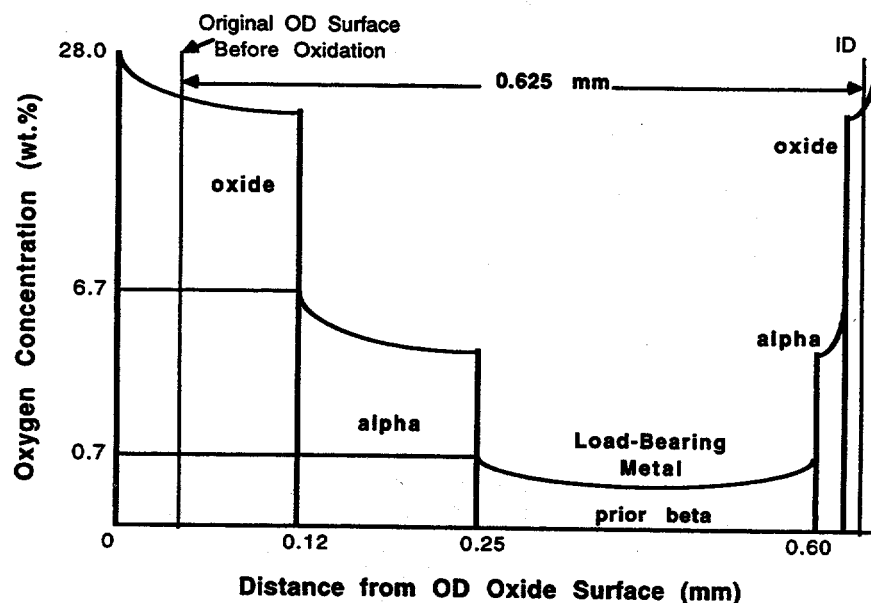


Figure 5.

Oxygen distribution in the oxide, α phase, and β phase layers at time of thermal shock during a LOCA.

These more sophisticated failure criteria are independent of not only cladding initial thickness and ballooning behavior but also oxidation temperature. However, to evaluate the thickness of the transformed- β -phase layer containing a critical oxygen concentration (e.g., <0.9 wt.%), detailed information on oxygen distribution such as that illustrated in Fig. 5 is needed. To calculate such oxygen distribution, basic oxidation kinetics such as oxide- and α -layer growth constants and oxygen diffusivity in β phase applicable to high-burnup cladding are needed. To obtain a sufficient data base on these kinetic parameters, extensive microstructural analyses will be conducted by means of optical and Auger electron spectroscopies. The latter technique is in particular

sensitive to local oxygen distribution in cladding.² The computer code developed previously to calculate cladding oxygen distribution will be also modified to reflect more accurately the oxidation kinetics applicable to high-burnup cladding.²

INTEGRAL LOCA TEST

The important thermal-hydraulic and material factors believed to influence high-burnup cladding failure under LOCA situations can be summarized as:

- (A) Initial cladding thickness.
- (B) Local oxide- and α -layer thicknesses from high-burnup operation (Fig. 6).
- (C) Hydrogen uptake and hydride distribution following high-burnup operation.
- (D) Wall thinning from ballooning and rupture and inner-side oxidation in the rupture region.
- (E) Peak oxidation temperature and time.
- (F) Cooling rate from the maximum cladding temperature through the β - and (α + β)- phase regions, i.e., from $\approx 980^\circ\text{C}$ to $\approx 815^\circ\text{C}$.
- (G) Thickness and structure of transformed- β -phase layer, Widmannstaetten structure in the colony.
- (H) Oxygen content and profile in the transformed- β -phase layer.

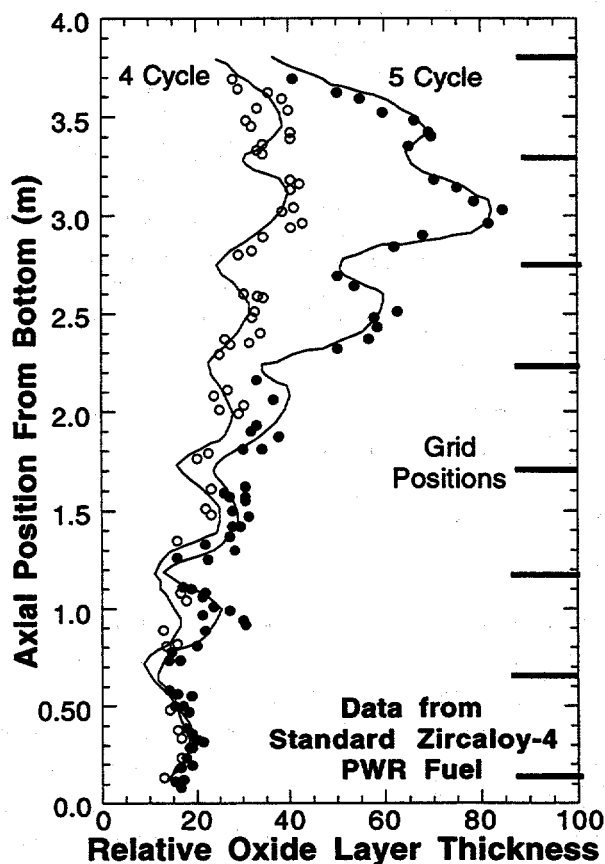


Figure 6.

Typical dependence of local oxide layer thickness on axial position and grid span for two high-burnup cycles (data from standard Zircaloy-4 PWR fuel).

- (I) Hydride distribution in the transformed- β -phase layer.
- (J) Residual hardening centers in the transformed- β -phase layer that are not annealed out during the high-temperature transient, e.g., complexes formed between impurities and irradiation-induced defect clusters and dislocation loops.
- (K) Irradiation-induced clusters or precipitates in the transformed- β -phase layer.
- (L) Thermal insulation by oxide layer.
- (M) Rewetting temperature of oxide layer.
- (N) Cladding constraint that prevents axial contraction.
- (O) Pellet-cladding bonding.

Many of these factors are strongly influenced by local oxidation, which is in turn strongly influenced by cladding alloy type, burnup, and axial location (grid span). Oxidation, and hence hydriding is strongly dependent on grid span position, as illustrated in Fig. 6. One can visualize from the figure that fuel cladding between the Nos. 6 and 7 spacer grids (from core bottom) would be most vulnerable to failure. This is because the local oxide layer thickness is at its maximum, and hence the thickness of the transformed- β -phase layer would be minimal after exposure to high-temperature excursion during a LOCA. Local hydriding is also at its maximum at the same grid span.

In consideration of the above factors, high-burnup cladding specimens ≈ 150 mm in length will be sectioned from several grid spans. Most of the integral LOCA tests will be conducted on low-Sn Zircaloy-4 PWR fuels irradiated to a burnup of ≈ 60 MWd/kg. A limited number of tests will be also conducted on standard Zircaloy-4 PWR fuels irradiated to a burnup of ≈ 60 MWd/kg as well as Zircaloy-2 BWR fuels of a burnup of ≈ 50 MWd/kg.

The ≈ 150 -mm-long cladding sections will be then defueled to remove UO_2 pellets, which will be replaced with Al_2O_3 pellets. This is necessary for safety during hot-cell testing. Both ends of the cladding sections will be then fitted into unirradiated Zircaloy sleeves, and the irradiated cladding and the unirradiated sleeves will be welded in a hot cell. Then the unirradiated Zircaloy sleeves will be sealed with Swagelok fittings to withstand pressurizing argon gas. This "single-rod" test assembly is shown schematically in Fig. 7.

Hot-cell integral LOCA tests on the "single-rod" test specimen will be similar to those reported in Ref. 2. Three types of tests will be conducted, which incorporate most of the essential factors that influence the failure behavior of high-burnup cladding. These tests are illustrated schematically in Figs. 7A, 7B, and 8, respectively, and described below.

- (A) Thermal-shock tests by direct quenching and by slow cooling and quenching at $< 700^\circ\text{C}$ following ballooning and rupture of the cladding and high-temperature oxidation (Figs. 1 and 7A)
- (B) Quenching test under combined thermal shock and bending moment applied in simulation of seismic loading (Fig. 7B)
- (C) Instrumented impact testing of the intact cladding specimens that survived the thermal shock (Fig. 8).

MECHANICAL PROPERTIES TEST

Various test methods used to determine the tensile properties and fracture toughness of high-burnup cladding have been evaluated. There have been increasing interests in the nuclear community in suitable hot-cell test methods to determine the mechanical properties directly

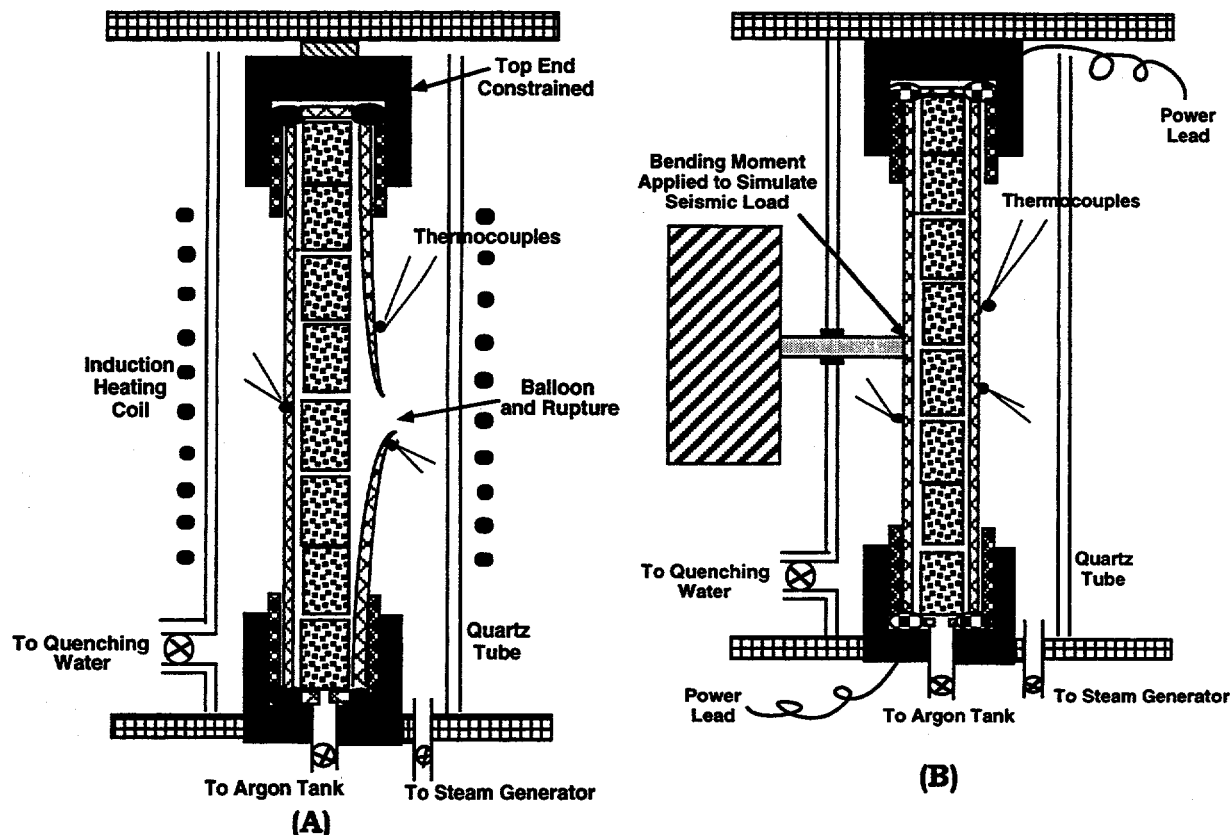


Figure 7. Schematic diagram of two types of simulated-LOCA test: (A) integral ballooning, oxidation, and quenching test; and (B) quenching test under combined thermal shock and bending moment applied in simulation of seismic loading.

applicable to LWR fuel cladding and steam generator tubing. Several new methods to test small tubular specimens have been reported recently.⁷⁻⁹ Methods used to determine the mechanical properties of thicker and larger-diameter pressure tubes (e.g., in a CANDU reactor) are mostly based on compact-tension specimens. This type of specimen geometry, however, is difficult to apply to high-burnup fuel cladding because of the small diameters and thin wall of cladding.

In this program, three types of tests will be conducted in separate hot cells to determine the fundamental mechanical properties of several types of high-burnup cladding, i.e., the modified ring-stretch test (MRST), electromagnetic ring-expansion test (ERET), and pin-loading fracture-toughness test (PLFT). Tensile properties and fracture toughness will be determined as a function of fuel burnup (50-60 MWd/kg), cladding fluence, irradiation temperature, oxidation, hydrogen uptake, hydriding, alloy composition, test temperature (23-400°C), and strain rate.

An attractive tensile testing method for high-burnup cladding is the "modified" ring-stretch test reported by Josefsson and Grigoriev.⁷ In a conventional ring-stretch test, a smoothly grooved ring specimen (grooved on diametrically opposite sides) is subjected to uniaxial tangential tension by two matching half-cylinders that are pulled apart inside the ring. The main shortcoming with this method is that significant bending moments develop in the grooved midsections. In the modified ring-stretch test, a third central piece is inserted inside the ring. The central piece is roughly in the shape of an I-beam, with spherical surfaces at the top and bottom of the "beam". The spherical surfaces maintain tight circular contact with the matching inner surfaces of the grooved

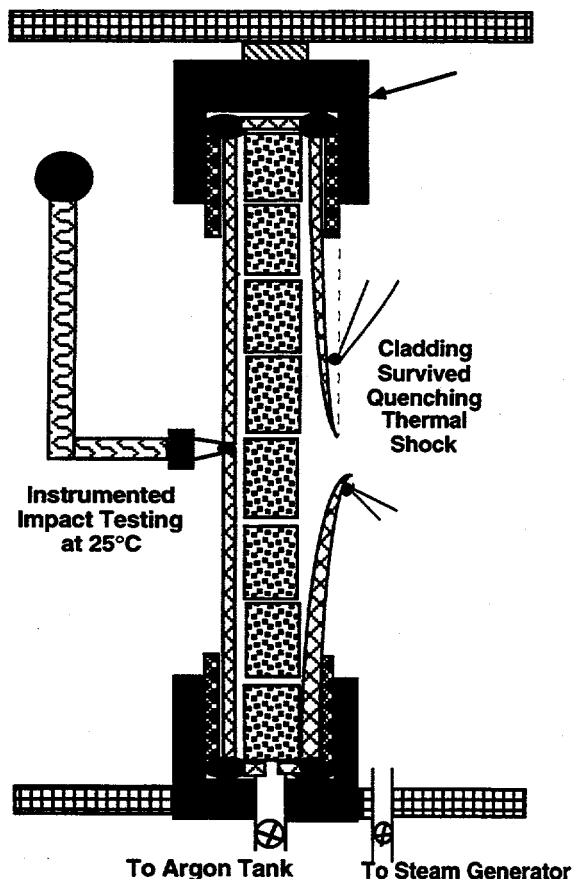


Figure 8.

Schematic diagram of instrumented impact testing of intact cladding specimens that survived thermal shock during simulated LOCA transient.

portion ("gage length") of the cladding. This effectively minimizes development of a bending moment in the grooved portion during stretching of the ring. It also minimizes contraction of the grooved portion in axial direction, which is the proper way to simulate tensile loading of high-burnup cladding during power-ramping and operational transient situations.

For application to reactivity-initiated-accident (RIA) situations, an attractive method to determine high-strain-rate tensile properties is the electromagnetic ring-expansion test.⁸ The test apparatus consists of a small Cu solenoid over which is placed a short ring specimen of high-burnup cladding. The solenoid is connected to a capacitor that is charged to a predetermined voltage; when the ignition switch is triggered on, the capacitor is quickly discharged through the solenoid. The high electrical current in the solenoid produces a strong magnetic field that in turn induces a current in the Zircaloy specimen, i.e., in the nonoxidized metallic portion of the cladding. Then, a Lorentz repulsion force acts between the cladding ring specimen and the solenoid coil. When this repulsion force reaches a value greater than the yield strength of the cladding material, the ring specimen expands rapidly. This test method is relatively simple and can easily produce the very high strain rates characteristic of RIA situations. A wide range of strain rates can be produced by adjusting the voltage and the total energy discharged. However, one drawback of the ERET method is that axial constraint of the cladding cannot be simulated in a simple way.

In gas-pressurization burst test of sealed cladding tube, the axial stress is one-half of the circumferential stress, and as a result, the cladding tube contracts freely in the axial direction. Because of this, and in the absence of localized deformation, circumferential strain determined from a burst test tends to be higher than that measured under pellet-cladding mechanical interaction (PCMI). Under the rapid PCMI loading in RIA situations, significant axial elongation

has been observed in simulation pulse tests in the NSRR and CABRI reactors. Therefore, applicability of burst test data to RIA situations is believed to be limited. At any rate, tube burst strain will be obtained as a by-product from the integral LOCA test depicted in Fig. 7A.

Measurement of fracture toughness of irradiated cladding requires the introduction of a sharp notch or precrack in the specimen. A precracking procedure of notched ring specimens in an Instron Model 8511 servohydraulic system is being developed. The precracked ring can be tested in tension by pin-loading, which is similar to compact-tension testing of larger pressure-tube specimens. In the pin-loading fracture-toughness test, the crack propagation behavior is similar to those under power-ramping and other operational transients; that is, crack propagates in the axial direction of the cladding. A plain-strain condition, which simulates PCMI loading during RIA or power-ramping situations, can occur because transverse (i.e., axial) contraction of the specimen during tension is prevented. This pin-loading fracture-toughness test method has been demonstrated successfully on irradiated fuel cladding in recent investigations.^{7,9}

SUMMARY

- A. Important thermal-hydraulic and metallurgical factors that should significantly influence the failure behavior of high-burnup cladding under loss-of-coolant-accident (LOCA) situations have been identified and evaluated.
- B. In consideration of these factors, three types of tests under simulated LOCA conditions have been identified, and test facilities are being designed, i.e., (1) thermal-shock tests by direct quenching and by slow cooling and quenching at <700°C following ballooning and rupture of the cladding and high-temperature oxidation, (2) quenching test under combined thermal shock and bending moment applied in simulation of seismic loading, and (3) instrumented impact testing of the intact cladding specimens that survived the thermal shock.
- C. Industry fuel design and operating conditions are expected to undergo further changes as more advanced cladding materials are developed. Under these circumstances, mechanical properties of high-burnup fuel cladding require further investigation so that results from studies on LOCA, RIA, operational transient, and power-ramping situations can be extrapolated to modified or advanced cladding materials and altered irradiation conditions without repeating major integral experiments in test reactors. To provide the most applicable data base on fracture toughness and tensile properties and mechanistic understanding of these properties, three types of tests have been identified, i.e., the modified ring-stretch test, electromagnetic ring-expansion test, and the pin-loading fracture-toughness test.

ACKNOWLEDGMENTS

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EXPERIENCE WITH INCOMPLETE CONTROL ROD INSERTION IN FUEL
WITH BURNUP EXCEEDING APPROXIMATELY 40 GWD/MTU

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Abstract

Analysis and measurement experience with fuel assemblies having incomplete control rod insertion at burnups of approximately 40 GWD/MTU is presented. Control rod motion dynamics and simplified structural analyses are presented and compared to measurement data. Fuel assembly growth measurements taken with the plant Refueling Machine Z-Tape are described and presented. Bow measurements (including plug gauging) are described and potential improvements are suggested. The measurements described and analysis performed show that sufficient guide tube bow (either from creep or yield buckling) is present in some high burnup assemblies to stop the control rods before they reach their full limit of travel. Recommendations are made that, if implemented, could improve cost performance related to testing and analysis activities.

EXPERIENCE WITH INCOMPLETE CONTROL ROD INSERTION IN FUEL WITH BURNUP EXCEEDING APPROXIMATELY 40 GWD/MTU

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Houston Lighting & Power Co., South Texas Project

I. Introduction

The South Texas Project Electric Generating Station (STPEGS) consists of two, 3800 MWth, four loop, Pressurized Water Reactors (Unit 1 and Unit 2) manufactured by Westinghouse. The two Units are essentially identical in design with each reactor core having 193 17x17 14 foot (XL) fuel assemblies (F/As) and 57 Rod Control Cluster Assemblies (RCCAs) operating on 18 month fuel cycles. In support of total fuel cost targets including spent fuel pool (SFP) discharge burden, the current F/A average burnup design is approximately 45 GWD/MTU. Unit 1 went on line on 30 March 1989 and Unit 2 went on line 4 November 1989. At the end of Cycle 6, Unit 1 had operated for a total of approximately 5 effective full power years (efpy).

Following a trip on 18 December 1995 during Unit 1's sixth fuel cycle (Cycle 6), three F/As under RCCAs with average burnups over 40 GWD/MTU experienced incomplete RCCA insertion. In subsequent testing at end of life, eleven RCCAs did not fully insert with two RCCAs inserting to twelve steps above rod bottom and 9 stopping at 6 steps above rod bottom [Leazar 1996]. The ability of all but the most reactive RCCA to fully insert and reach the dash pot within a required time limit is a typical Safety Analysis assumption for Shutdown Margin. Although STPEGS had not previously experienced incomplete control rod insertion following a reactor trip, twenty one other plants have had incomplete RCCA insertions for various reasons prior the incomplete insertion experience at STPEGS Unit 1 [Cloninger 1996].

Based upon STPEGS experience, incomplete insertion can be avoided by limiting current F/A designs to less than approximately 40 GWD/MTU with normal operating times when located under RCCAs. However, limiting F/A burnup at RCCA locations reduces core design flexibility and initially results in more discharge F/As to the SFP. These limitations on the options for placement of F/As, as specified in the core loading patterns, will lead to lower design burnup and higher fuel costs.

Unlike other aspects of reactor safety, such as thermal-hydraulics and fuel pin modeling where standardized methodologies like RELAP5 and FRAP can be easily implemented by the Utility engineer, standardized rod drop dynamics and F/A structural analysis methods are not readily available, if at all. Also, data are not generally available, although Jelovich [1977] describes how RCCA drop traces from operating reactors using DRPI can be analyzed to derive velocity vs. time and bow measurements have been made on operating reactor fuel at individual reactor sites. Developing analytical methods and performing tests to determine root cause are costly to both the Utility and the fuel Vendor. Experience in testing and analysis can help show where cost performance improvements are possible and should be fed back to investigators in the Utility and the Vendor.

II. Description of STPEGS

During Cycle 6, the Unit 1 core operated with three different XL F/A designs. The distinguishing features of the designs are successive variations on the basic 17x17 XL fuel design. The oldest fuel design uses all Inconel grids (XL Standard); the next oldest design (XLR) employs an extended burnup skeleton, debris filter bottom nozzles, and removable top nozzle; the newest design (V5H) includes intermediate Zirconium grids, and smaller diameter guide tubes. The F/As are operated at an average temperature of 589 F and total core flow rate of approximately 400,000 gpm. The F/A and RCCA are illustrated in Figure 1, the guide tube geometry is shown in Figure 2 [Cloninger 1996]. The nominal guide tube wall thickness is approximately 0.017 in [Cloninger 1996].

The basic reactor core assembly is defined by mating pins and holes in the upper and lower core plates. The core plates clamp the F/As vertically with the clamping force provided by the F/A top nozzle leaf springs. The clamping force is maximum when the reactor is cold (approximately 2800 lbf) and decreases to about 1400 lbf [Cloninger 1996] when

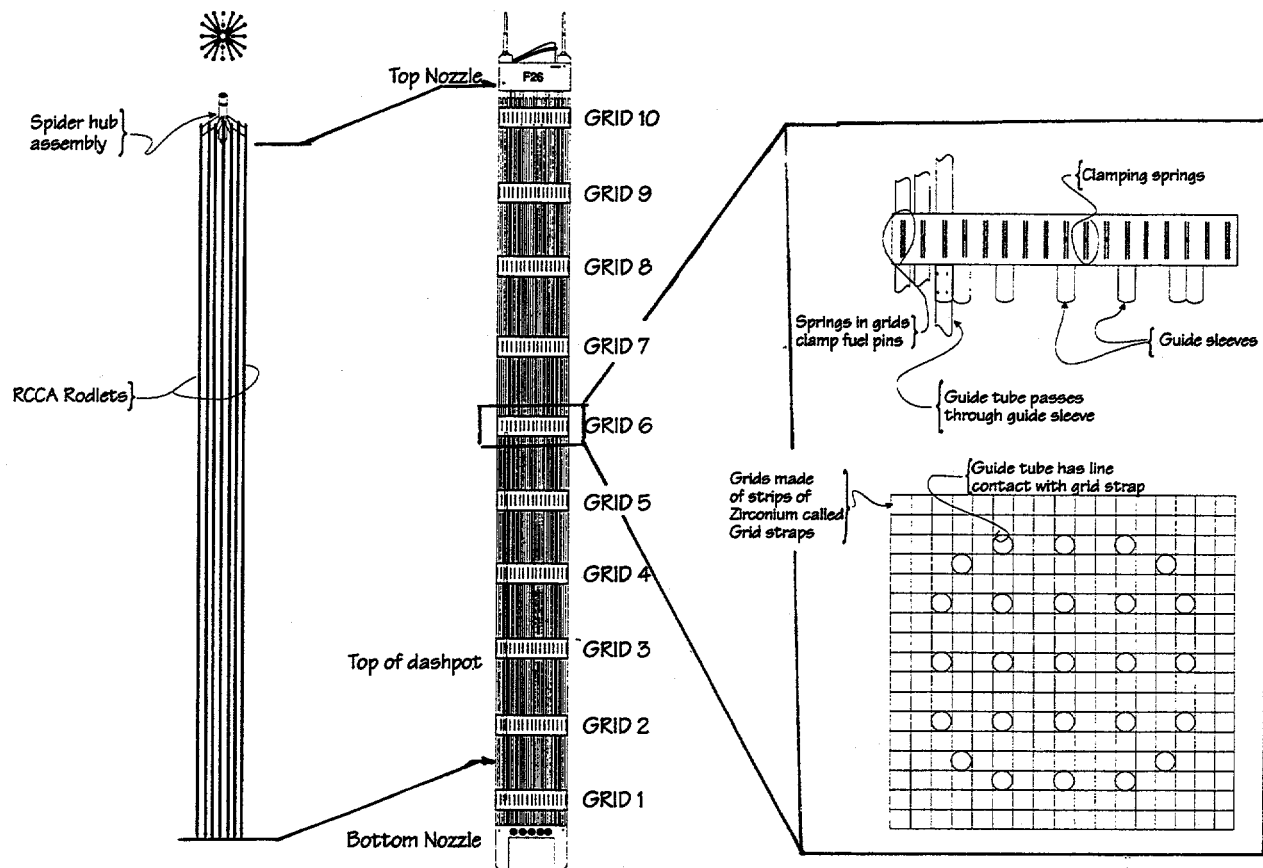


Figure 1. STPEGS Fuel and RCCA nomenclature.

the reactor is at operating temperature. When the core is assembled, the nominal distance between adjacent F/A grids is approximately 0.04 in. Around the periphery of the assembled core is the core former which forms a rigid wall extending from the lower core plate to the upper core plate. The F/A grid clearance to the core former is approximately 0.08 in.

Fifty seven RCCAs are used primarily to control flux shape and insert immediate reactivity when required during a reactor transient. Each RCCA consists of a spider hub assembly consisting of the mechanical interface to the RCCA drive shaft, a shock absorbing spring, and arms that locate the 24 RCCA rodlets in the F/A guide tubes. The rodlets are permanently attached to the spider hub assembly arms and consist of a stainless steel clad absorber. The RCCAs currently installed use Silver-Indium-Cadmium absorber material and have been in use since Cycle 2 (approximately 4.2 efpy). The current RCCAs replaced the initial RCCAs that used Hafnium absorber material. The nominal RCCA clearances to the guide tube wall are approximately 0.035 in above the dash pot and 0.006 in in the dash pot [Cloninger 1996].

The RCCAs can be operated in two basic modes during normal operation, Manual or Automatic. Both Units have been used for base load service since initially going on line (i.e. essentially all the cycle energy has been derived at the full power operating point) and therefore the RCCAs are only moved a few steps in a month in either mode of operation. The RCCAs are grouped into nine banks as follows: Shutdown Banks: SA, SB, SC, SD, SE, and Control Banks: CA, CB, CC, and CD; and are arranged as shown in Figure 3 and the accompanying table.

Direct measurement of RCCA position is by a Westinghouse Digital Rod Position Indication (DRPI) system that uses a series of electrical coils through which the RCCA drive shaft passes. The DRPI operates on the principle the reactive

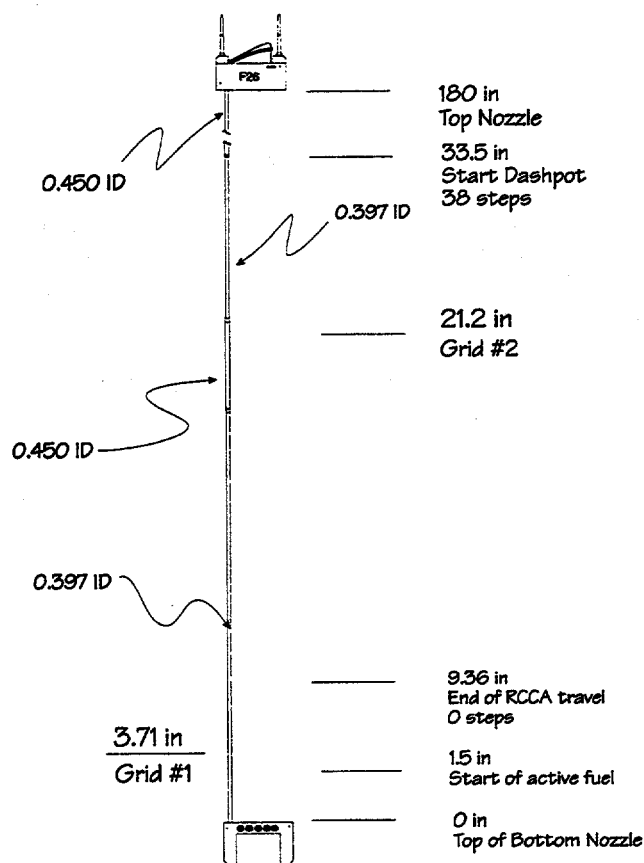


Figure 2. Guide tube geometry.

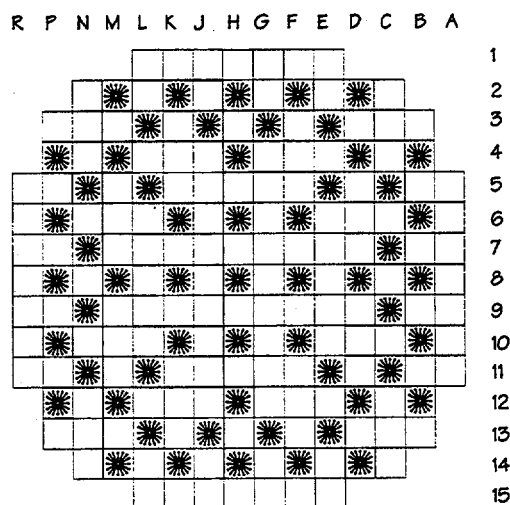


Figure 3. RCCA locations in the STPEGS reactor.

Bank	Core Location							
SA	P04	P12	M02	M14	D02	D15	B04	B12
SB	N07 ¹	N09 ²	J03	J13	G03	G13	C07 ²	C09 ¹
SC	N05	L13	E03	C11	-	-	-	-
SD	N11	L03	E13	C05 ²	-	-	-	-
SE	M08	H04	H12	D08 ²	-	-	-	-
CA	L05	L11	H06	H10	F08	K08 ²	E05	E11 ²
CB	K02	K14	F02	F14	-	-	-	-
CC	P08	K06	K10 ²	H02	H14	F06 ²	F10 ¹	B08
CD	M04	M12	H08	D04	D12	-	-	-

¹ Locations of stuck RCCAs after 12/18/95 Unit 1 trip.

² Locations of additional stuck RCCAs at the end of Cycle 6 testing.

impedance of individual coils is different when the RCCA drive shaft is in, or out of, the coil. The DRPI system can discriminate RCCA position to within four steps ($5/8$ in/step) within measured spans. The DRPI measurement spans the entire length of travel for Control Banks, but not Shutdown Banks. In the case of Shutdown Banks, the center portion of the rod travel is not measured because, unlike the Control Banks which can be operated at any position of their travel, Shutdown Banks are operated either fully withdrawn or fully inserted. The normal full operating range of STPEGS RCCAs is between 0 steps withdrawn to 259 steps withdrawn from the bottom of travel. The Shutdown Bank DRPI measurement spans are between 0 steps and 18 steps withdrawn and between 238 and 259 steps withdrawn.

III. Analytical modeling

Immediately following the trip on 18 December 1995, the rod drop dynamics and F/A structural performance were studied to determine the most likely reason for the incomplete insertion behavior. Rod drop analysis was directed at simulating the drop process and then imposing different types of possible slowing and sticking models. The main areas studied were narrowing of the clearance between the RCCA and the guide tube wall and sliding friction from guide tube

bending. Simple structural analysis was directed at determining the most likely failure mode(s) for the F/A guide tubes. Because the F/A design is complicated and interaction among surrounding F/As in the assembled core are important to the F/A performance, it does not lend itself easily to simplified analysis. On the other hand, basic design theory can be applied to help understand general behavior and failure mechanisms. The maximum RCCA and guide tube deflection prior to yield was also determined for comparison to measurement data and to assess the potential for RCCA clad failure.

III.A. Rod drop model

A force balance equation describing the velocity history for a control rod drop was written from first principles of viscous dampening in the three basic sections of the guide tube, i.e. before the dash pot, after the dash pot, and following spider hub assembly spring contact. The guide tube and dash pot geometries are shown in Figure 2 and the forces acting on the RCCA during a drop are shown in Figure 4. As shown in the figures, the twenty four RCCA rodlets must go down into a 0.450 in inside diameter (ID) guide tube section from about 180 in out until they are about 33.5 inches from the bottom, displacing coolant as they fall. At this point, the guide tubes narrow to 0.397 in ID to form the dash pot. At the bottom of the dash pot is a small metering orifice that will allow a small amount of water to escape. For the purpose of modeling the drop process, flow through the orifice can be ignored. On the other hand, four small holes in the tubes above the start of the top dash pot allow displaced water to escape ahead of the falling RCCA.

The force balance equates RCCA acceleration to the sum of the: wall shear in the guide tube region above the dash pot; bluff body flow losses on the hub; wall shear in the dash pot region; gravity; sliding friction from RCCA-to-guide tube wall interference; and RCCA hub spring interaction with the F/A top nozzle:

$$\frac{d^2x}{dt^2} = -\frac{C_1 \hat{x}}{M} \frac{dx}{dt} - \frac{C_2 \left(\frac{dx}{dt}\right)^2}{M} - \frac{C_3 \bar{x}}{M} \frac{dx}{dt} + g - \frac{\mu_{fr} N}{M} - \left(\frac{k}{M} \xi + \frac{F}{M}\right),$$

where:

M is the RCCA and drive shaft mass,

k is the RCCA hub spring's spring rate,

F is the RCCA hub spring preload,

g is gravitational acceleration,

N is the applied normal force between the RCCA and Guide Tube,

μ_{fr} is coefficient of friction for 304 stainless - Zirc 4,

$\hat{x} = x; x < L$

$\hat{x} = L; x \geq L,$

$\bar{x} = 0; x < L$

$\bar{x} = x - L; x \geq L,$

L is the guide tube length,

$\xi = 0; x < \hat{L}$

$\xi = x - \hat{L}; x \geq \hat{L},$

$\hat{L}$ is the distance to the point of RCCA hub spring contact,

$C_1 = \frac{\pi d_{rcca} \mu}{\delta_{gt} M}$, μ is the coolant viscosity, d_{rcca} is the outside diameter (OD) of the RCCA, δ_{gt} is the annular gap between the RCCA and the guide tube above the dash pot,

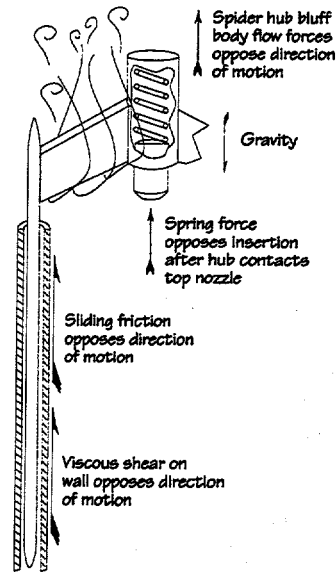


Figure 4. Illustration of the forces acting on an RCCA during a rod drop.

$C_2 = \frac{C_D A_{hub} \rho}{2}$, A_{hub} is the spider hub cross sectional area, ρ is the coolant density, C_D is the drag coefficient for a falling cylinder,

$C_3 = \frac{\pi d_{RCCA} \mu}{\delta_{dp} M}$, and δ_{dp} is the annular gap between the RCCA and the guide tube in the dash pot.

The force balance doesn't take into account flow of displaced coolant (particularly in the dash pot) up past the RCCA as it falls as illustrated in Figure 5. Instead, C_1 and C_3 are based upon Couette flow assuming a straight line velocity gradient from the RCCA wall to the guide tube wall. Clearly, upward coolant flow in the annulus between the RCCA and guide tube walls will cause steeper velocity gradients at the RCCA wall than the linear gradients assumed and thereby cause higher shear forces than assumed. An additional assumption is that the pressure difference from the rodlet tip to the point where flow is exhausted out the top is negligible. These assumptions have been relaxed recently by Kee [1996]. Kee's results show that, under the assumption of no flow out of the metering orifice at the bottom of the dash pot, the shear force is strictly linear with rodlet velocity. Finally, when comparing the equation to measurement data, the equation does not take into account the missing coils in the center of travel of Shutdown Banks as discussed previously.

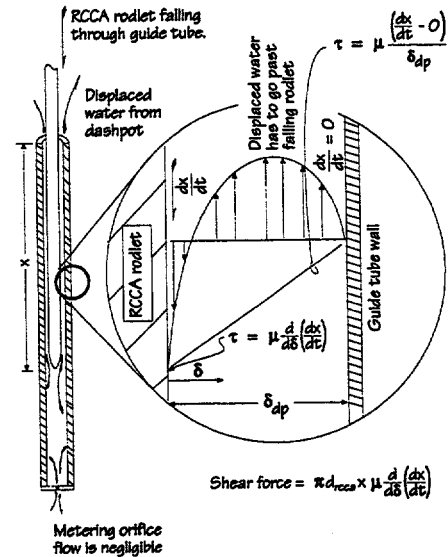


Figure 5. Illustration of flow and velocity profile in the dash pot.

Based upon a preliminary review of the plant drawings and rod drop data taken following the incomplete rod insertion during the 18 December 1995 trip, it was hypothesized that the sticking was primarily due to either a reduction in clearance or interference from guide tube bending. In order to study the hypothesized effects, the force balance equation was solved using TutSim [Reynolds 1987]. TutSim is a commercially available software product that can be used to solve equation(s) by setting up the equation(s) in a model consisting of a series of computational blocks that form an algebraic loop. Integration options can use either Adam's-Bashforth or Euler to advance the loop. The force balance equation was advanced using the Adams-Bashforth method on a 0.001 second time step. The model was implemented on an IBM 755CD ThinkPad running PC-DOS under OS/2 Version 3.0.

The TutSim model was designed to allow the user the ability to independently or simultaneously change the coefficients C_1 , C_2 , C_3 and μ_f . Also, it allows a first order approximation to the increase in sliding friction as the RCCA entered the dash pot; i.e. $\mu_f = f(x)$. Using the equations previously presented, $C_1 = 0.46$ (lb/in-s), $C_2 = 0.11$ (lb/in-s), and $C_3 = 2.6$ (lb/in-s). The model coefficients were initially tuned to obtain agreement with the timing of dash pot entry and spider hub spring contact in a normal rod drop (normal insertion case). The tuned coefficients resulted in values of $C_1 = 1.7$ (lb/in-s), $C_2 = 10.2$ lb/in, and $C_3 = 245$ (lb/in-s). The large difference between analytical and tuned values is most likely due to neglecting the pressure in front of the rodlet and the assumption of linear velocity gradient across the guide tube-RCCA annulus. Using the tuned coefficients, cases were made to investigate the effect of changing the dampening coefficient and the sliding friction by modifying the coefficients C_3 and μ_f .

III.B. Sliding friction cases

Sufficient guide tube bow would cause RCCA rodlet strain which in turn would generate normal forces between the RCCA rodlet and the guide tube wall. This effect was tested as an incomplete rod insertion mechanism in the RCCA force balance equation for assumed guide tube deflections within the allowable in-reactor envelope.

A simple method to evaluate the sliding friction is to assume uniform loading on a simply supported beam and use the maximum achievable deflection (i.e. grid clearance between F/As) as the maximum beam deflection to calculate the bending force (N):

$$N = \frac{384 \Delta}{5 L^3} (E_{ss} I_{ss} + E_{ag} I_{ag})$$

where:

Δ is the maximum deflection in the span (approximately twice the inter-grid spacing or about 0.08 in),

L is the span between grids (approximately 19 in),

E_{ss} is the modulus of elasticity for 304 stainless,

I_{ss} is the RCCA clad moment of inertia - $I_{ss} = \frac{\pi}{64} (d_{rcca}^4 - (d_{rcca} - 2t_{rcca})^4)$, t_{rcca} is the wall thickness,

E_{ag} is the modulus of elasticity for Ag-In-Cd poison, and

I_{ag} is the RCCA absorber moment of inertia - $I_{ag} = \frac{\pi}{64} d_{ag}^4$, d_{ag} is the Ag-In-Cd poison diameter.

Note that the spans above the dash pot will not have as large a value for Δ because there is a great deal more clearance between the RCCA and the guide tube in that region. This is true for all STPEGS F/A designs, but the V5H design has less clearance than previous designs.

An interesting feature of the friction equation above is that the friction force developed is inversely proportional to the third power of the span and linear in the deflection. Therefore, a bow that extends over several spans (or simply a larger fraction of the F/A length) that has a large deflection (Δ), should have significantly less sliding friction than a bow with less deflection that extends over a shorter span.

III.C. Viscosity cases

A possible scenario for incomplete RCCA insertion is RCCA rodlet swelling. In this case, clearance between the RCCA rodlet outer diameters and the dash pot inner diameters (δ) would be reduced. As previously shown in the equation for C_3 , (assuming viscous dampening in the dash pot is developed from the fluid shear stress between the moving RCCA and the stationary dash pot wall), C_3 should be approximately inversely proportional to δ_{dp} .

IV. Structural analysis

STPEGS F/As are constructed with twenty five thimble tubes that extend from the bottom nozzle to the top nozzle. Twenty four of the thimble tubes (guide tubes) accept and guide the RCCA rodlets and one tube in the center of the F/A accepts instrumentation (instrumentation tube). The thimble tubes carry the sum of the weight (P) of the fuel and the compressive force of hold down springs located on the F/A top nozzle. The F/A top nozzle hold down springs are compressed during normal operation by the reactor internals upper core plate. As stated before, because of differential thermal expansion between the reactor internals and the F/A thimble tubes, the net effect of the differential thermal expansion is to cause more F/A top nozzle spring compression at lower temperatures. The fuel rods are vertically located between the F/A top and bottom nozzles and maintained in a square matrix by ten grid assemblies. Each of the ten grid assemblies transmit the vertical load of the 264 fuel rods to the 25 F/A thimbles by clamping the fuel rods with springs on each side of the fuel rod's cell and mechanical joints on the thimbles. The fuel rod clamping springs accommodate differential thermal expansion/contraction and irradiation growth. Any unrelieved thermal/irradiation growth strains are carried through the grids to the guide tubes as bending loads. While the grids transmit the fuel rod weight to the thimbles, they also act as horizontal restraints for the thimbles and can carry some shear load. Nominal coolant flow rate up through the F/A is such that it tends to relieve the fuel rod weight.

The thimbles were evaluated to determine if they should be considered either as compression members or columns based upon the so-called slenderness ratio (sr) given by $sr = L'/r$, where L' is the effective column length and r is the minimum

radius of gyration ($r = \sqrt{I/A}$). In this analysis it was assumed the F/A could be thought of as a series of stacked columns that start and end at each grid location. The grids are assumed to carry all bending (i.e. "fixed" boundary condition). Depending upon the nature of the bending boundary condition (i.e. guided or unguided restraints - as illustrated in Figures 6 and 7), L' is taken as either the inter grid span or 1/2 the inter grid span. The maximum value of the slenderness ratio for all current STPEGS fuel designs based upon buckling assuming unguided restraints is approximately 127, indicating the thimble will act structurally as a column and buckling (yield) will occur significantly below the yield strength. On the other hand, assuming guided restraints, the slenderness ratio is 63, which would be out of the slender range and the guide tube would act like a compression member with failure taking place at the yield strength.

As stated above, when taken as a compression member, failure will occur when the yield strength of the guide tube is exceeded. The stress in this case would be calculated by simply dividing the total load by the guide tube area. When taken as a column, a simple analytical approach to evaluate performance is the well-known secant formula:

$$\frac{P}{A} = \frac{\sigma_y}{1 + \frac{ec}{r^2} \sec\left(\frac{L'}{2r} \sqrt{\frac{P}{AE}}\right)}$$

where:

I is the dash pot area moment of inertia,

P/A is the unit load (not normal stress),

A is the dash pot cross sectional area,

$\frac{ec}{r^2}$ is the eccentricity ratio (0.25 used typically in column design),

c is 1/2 the tube OD, e is the eccentricity of the load,

σ_y is the yield strength of the thimble tube material (approximately 45×10^3 psi), and

E is Young's modulus for the thimble material (approximately 14×10^6 psi).

If the guide tube bend radius, ρ , is known, maximum elastic stress, $\sigma_{\max}$, in the guide tube is given by the stress-strain relationship, $\sigma_{\max} = E c / \rho$ where c is the guide tube or dash pot OD. This relationship would be useful for deducing residual thermal and growth strain effects on F/As where guide tube bow measurements are available and creep strain is negligible.

In general, the F/A guide tubes will be subjected to bending loads (M) from coolant cross flows, differential thermal/irradiation growth in the fuel rods, and possibly interaction with adjacent F/As. Exact quantification of these bending loads is effectively impossible. If quantified, they could be added directly to the normal stress via the relationship $\sigma = M c / I$.

V. Measurements

Several different tests and measurements were undertaken following the 18 December 1995 trip to help understand the incomplete RCCA

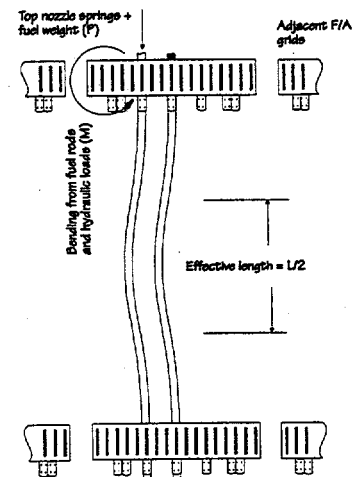


Figure 6. Buckling mode for guided restraints.

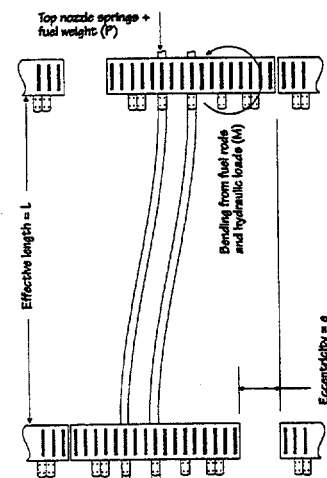


Figure 7. Buckling mode for unguided horizontal restraints

insertion behavior. RCCA drop tests (rod drops) were performed mid-way between the end of Cycle 6 (18 May 1996) and 18 December 1995. Rod drops were also performed at the end of Cycle 6. After the refueling outage that started on 18 May 1996, testing was performed in the SFP on 19 F/As. The measurements are described below.

V.A. Rod drop measurement

Rod drop measurement is an approximate measure of the RCCA velocity history when the RCCAs are dropped from the full out position. Although the measurement is not calibrated for velocity, for a short time just prior to dash pot entry, the RCCA is traveling at terminal velocity and Jelovich's method as described previously can be used to establish the velocity. The basic rod drop measurement is accomplished by recording the voltage generated in the DRPI coil stack of each RCCA during the drop as shown schematically in Figure 8. The rod drop measurement is primarily used to determine the time elapsed from the initiation of stationary gripper voltage decay to the time of dash pot entry but can also indicate the velocity history of a particular RCCA during a rod drop. As discussed previously, the center of travel is not measured in Shutdown Banks, and therefore measurement curves for Shutdown bank rod drops have a different characteristic than Control Banks. Characteristic curves for Shutdown bank and Control Bank rod drop measurements are shown in Figures 9 and 10.

Although DRPI must be de energized for position measurement during the time the rods are actually falling, it is re energized immediately after the drop is complete and the positions of any stuck RCCAs can be determined essentially immediately after the drop.

Performance of the rod drop measurement requires substantial effort and cost to the Utility especially when performed mid-cycle. Besides revenue loss over approximately one day, the required plant shutdown requires additional liquid waste processing and substantial support from the operations staff to perform the shutdown.

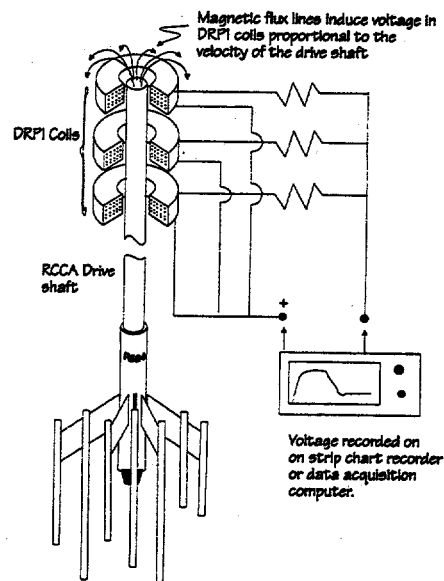


Figure 8. Schematic of DRPI rod drop measurement.

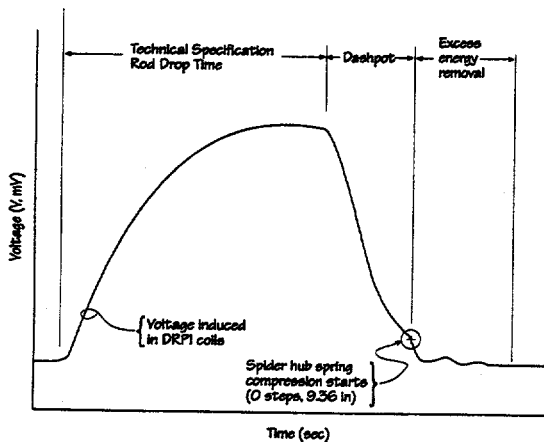


Figure 9. Typical normal Control Bank rod drop trace.

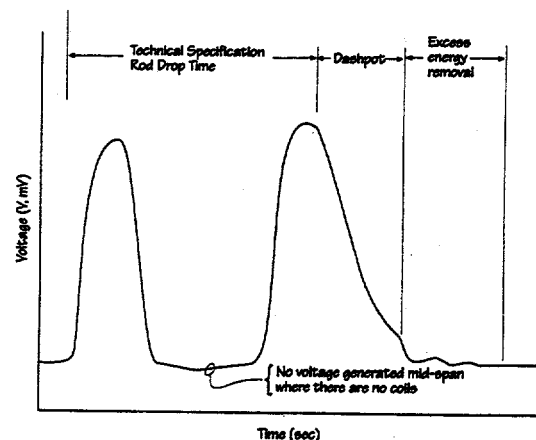


Figure 10. Typical normal Shutdown Bank rod drop trace.

V.B. Plug Gauge Measurement

One of the most important measurements related to fully establishing the Root Cause for the RCCA behavior in Unit 1 F/As is measurement of the amount of bow in the guide tubes. The direction of bow, the nature of the bow at the grids (i.e. the second derivative of the curvature), and the bow amplitude are the parameters needed to verify structural model behavior and definitively establish the Root Cause (i.e. guide tube bow). The best bow measurement would give a continuous bow amplitude and direction over the length of the guide tube. The objective of plug gauge measurements is to partially help establish how much bow may be present at a certain elevation. Plug gauges can also detect ovality, crippling, or any obstruction that reduces the ID of the guide tube. Plug gauge measurements are made by inserting solid cylinders into the guide tube until they are stopped by either going to the bottom of the guide tube ("GO") or before reaching the bottom of the guide tube due to the guide tube geometry ("NO GO").

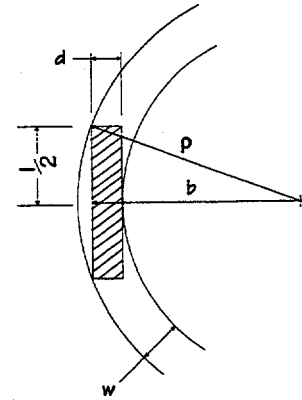


Figure 11. Geometry of the plug gauge in a tube.

As described below, several plug gauges of different dimensions must be used in each guide tube to adequately characterize bow and therefore, the nature of the plug gauge process requires inserting and removing many of these gauges into and out of the F/A then out of the water for gauge changes. Because the gauges in some cases are close to the ID of the guide tube, they tend to scrape off highly activated oxide from the guide tube ID which in turn gets carried up to the Operating deck resulting in the spread of contamination. In order to control the spread of contamination, significant Health Physics staff support is required.

The plug gauging method is effected by mounting the plug gauge on a marked, solid rod. The solid rod has a smaller diameter than the plug gauge, but will carry a bending moment. Therefore, the point at which the plug gauge stops is partially dependent upon the amount of any bending impressed upon the plug gauge from the mounting rod. The moment impressed on the plug gauge is in turn dependent upon the guide tube geometry above the gauge and therefore introduces a variable amount of uncertainty into the measurement.

It is clear that the plug gauge method will indicate the first occurrence of a limiting obstruction while any successive occurrences of the same obstruction further down the guide tube will GO a smaller diameter gauge of the same length. Also, it is clear that the plug gauge method alone will only indicate the presence of an obstruction without indicating the type. Finally, plug gauges can not be used to establish the direction of a bend.

Resolution of the plug gauge measurement is set by the number of plug gauges available in two basic dimensions, length and OD. Using plug gauges, the radius of curvature, ρ , of a circular tube with an ID, w , that stops a plug gauge of length,

l , and OD, d , (illustrated in Figure 11) can be calculated from $\rho = \frac{l^2}{4 + (d - w)^2}$. Also the total deflection, δ , at a F/A grid with a grid-to-grid span length L and bend radius ρ can be approximated from $\delta = \rho (1 - \cos \theta)$ where $\theta = \sin^{-1} \frac{L}{2\rho}$.

The plug gauge equations above assume the guide tube maintains a circular cross section when bent. For shorter bend radii, the circular assumption clearly would not be valid. Because STPEGS F/As have twenty four guide tubes, many separate probe measurements are required for each F/A in order to assess the bow condition. Although not all plug gauges need to be used in each guide tube, normally at least twice the minimum number are required and up to three times the minimum can be needed. Depending upon the skill of the technician and the number of plug gauges required up to a full shift may be required to complete a plug gauge measurement on one F/A. Typically, the measurements on one F/A can be completed in about one half of a 10 hour shift.

V.C. Length measurement

F/A growth increases the applied load from the hold down springs and is therefore an important parameter (combined with hold down spring rate) for computing the F/A axial compressive load in the assembled reactor core. Estimation of growth requires knowledge of the initial length to calibrate the measurement equipment. STPEGS used two methods to estimate Unit 1 Cycle 6 F/A growth. One method used Vendor supplied equipment and the second method used the Refueling Machine Z-Tape.

The Vendor length measurement requires addition of length measurement equipment into the SFP. In addition to inserting the length measuring equipment into the SFP, the Vendor method requires moving the F/A from its storage location to the measurement location and back again.

During the Unit 1 core off load in May, the elevation was recorded from the installed measuring device on the Refueling Machine, referred to as the Z-Tape, as each F/A removed from the core was put in the Refueling Transfer Cart Upender. This elevation is noted by the Refueling operations staff as each F/A is unlatched in the Refueling Transfer Cart Upender to verify the Refueling Machine Mast is fully down, but is not normally recorded. The Refueling Machine Z-Tape precision is approximately 0.01 ft but is "uncorrected" since thermal strains in the measuring equipment are not included in the result. Thermal strain could be accounted for in the measurement, but would require extra temperature measurement equipment local to the Containment Upender. The measurement standard for this method is the DUMMY F/A which is an unirradiated, non-fissionable prototype F/A. The DUMMY F/A is placed in the Refueling Transfer Cart Upender during normal check-outs for refueling operations and can be measured at that time. Using the Refueling Machine Z-Tape, the entire core can be sampled for length by simply recording the Refueling Machine Z-Tape elevation during a refueling core off load.

V.D. F/A Bow measurements

As discussed previously, one of the most important measurements related to fully establishing the Root Cause for the RCCA behavior in Unit 1 F/As is measurement of the amount and direction of bow in the guide tubes. The F/A bow measurements partially help to establish the location of the guide tube at the ten discrete grid locations. F/A bow measurements were made by viewing and recording a thin (approximately 1/8 in diameter) wire rope attached by a fixture to the F/A bottom nozzle and pulled vertically up to the top nozzle. The recording was reviewed to estimate deflection at each grid via scaling a known dimension from the grid to the observed difference between the wire rope and the center of each of ten grids. The process requires moving the F/A from its storage location in the SFP to an open area of where it can be raised and lowered for the purpose of video recording. At least two faces 90° from each other need to be measured to get the total F/A bow. The method used produces measurement of unconstrained bow, as opposed to bow that would exist in the assembled core when F/A bow is more than the nominal clearance between F/As.

Bow measurements typically allow for small misalignment between the true center of the F/A and the wire rope at bottom and top of the F/A. Because of the small misalignment, an artificial tilt could be introduced in the bow measurement, B , at any elevation, Z . The artificial tilt can be removed by rotating the measurement coordinate system. The easiest way to accomplish this is to shift the measurement to be 0.0 at either end of the F/A (i.e. grid 1 or grid 10) then rotate all the measurements (taken as a vectors: $B_y^T = [Y, Z]$, $B_x^T = [X, Z]$ with Y and X referring to the two measurement directions taken 90° from each other), by the angle to the opposite end grid (i.e. assume that the measurement is 0.0 at the base grid location). The grids occur at elevations, 8.9, 26.9, 45.5, 65.3, 85.1, 105, 125.8, 144.6, 164.4, and 181.7 inches from the bottom of the bottom nozzle. Therefore, rotating the measurement system about grid would amount to shifting the origin to grid 1, then computing the rotation angles, θ : $\theta_y = \tan^{-1} \left(\frac{Y_{10} - Y_1}{Z_{10} - Z_1} \right)$, $\theta_x = \tan^{-1} \left(\frac{X_{10} - X_1}{Z_{10} - Z_1} \right)$ the rotation matrices: $R_{y,x} = \begin{bmatrix} \cos \theta_{y,x} & \sin \theta_{y,x} \\ -\sin \theta_{y,x} & \cos \theta_{y,x} \end{bmatrix}$, and applying the rotation to each of the remaining nine measurements: $\hat{B}_{y,x} = R_{y,x} B_{y,x}$. In this case, $B_y = \begin{bmatrix} Y \\ Z \end{bmatrix} - \begin{bmatrix} Y_0 \\ Z_0 \end{bmatrix}$ and $B_x = \begin{bmatrix} X \\ Z \end{bmatrix} - \begin{bmatrix} X_0 \\ Z_0 \end{bmatrix}$ with the index 0 referring to the measurement at the first grid. Once the measurements are aligned to the same coordinate system, the direction of bow and length of bow at any grid can be established.

A bow measurement method proposed by Bear [1996] is based upon strain gauge technology and would be a more accurate and efficient method for obtaining guide tube bow, as opposed to F/A bow, if it could be qualified on irradiated F/As. The method produces essentially continuous guide tube bow direction and amplitude over the length of the guide tube. The method has been tested on unirradiated F/As and shown to be accurate and feasible however it has not been attempted on irradiated F/As. If qualified, Bear's method would eliminate the need to move F/As, produce precisely the data needed to characterize guide tube bow, only require one pass per guide tube, and eliminate the need for F/A and plug gauge measurements on this issue. An additional benefit is that the F/As would be partially constrained in the SFP cells, which would more closely represent the assembled core configuration. Note that plug gauge methods would still be needed for screening, for instance on other issues. Additionally, because the tooling would not have to be completely removed from the water after each measurement, the risk for spread of contamination is greatly reduced and therefore Health Physics staff support would be significantly reduced.

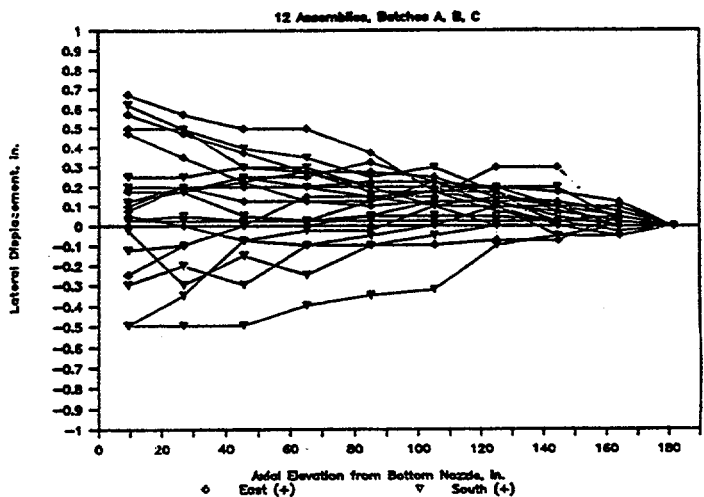


Figure 12. Bow measured on several assemblies following Cycle 1.

VI. Results

As discussed previously, the most important parameter measured is F/A guide tube bow. Unfortunately, results from the tests performed on the F/As that had incomplete RCCA insertion could not be included in here due to the Vendor's request. Therefore, to illustrate F/A bow behavior, bow measurements from a campaign conducted by the Utility and Babcox & Wilcox after offload of the Unit 1 Cycle 1 core Kennard [1989] are included in here. Unlike the Vendor measurements, Kennard's bow measurements are not corrected to remove any angle in the wire rope as discussed in Section V.D. Results are shown on Figure 12 (the Utility has provided the Vendor the corrected Vendor bow measurements from Cycle 6). As shown in the figure, the grids do not remain in alignment like guided restraints (this behavior is typical of the measurements made on the Cycle 6 assemblies as well). Instead, the grids are displaced from vertical alignment by as much as 0.5 in. As discussed in Section IV, the slenderness ratio would therefore be 127 (unguided restraint) and the guide tubes can be analyzed as a series of stacked columns. Another observation of the bow measurement is that all the F/As measured will exert lateral loads on adjacent F/As when assembled in the core because the measured bow is much larger than the in-core grid-to-grid clearance. This observation is additionally substantiated by experience with core assembly during refueling.

Using the maximum stress equation described in Section IV, the maximum stress corresponding to the guide tube curvature required to stop one of the plug gauges in both F/As F26 and F41 is 84,000 psi which is over the yield strength of the guide tube by about a factor of two. Although the maximum stress equation is based upon the assumption of no ovality in the measured tube, it is likely that plastic deformation has taken place in the dash pots of these F/As. Because only one of the plug gauges used was capable of detecting plastic deformation in the dashpot, the upper section of the guide tube could not be evaluated using the plug gauge method. An important observation is that STPEGS Unit 1

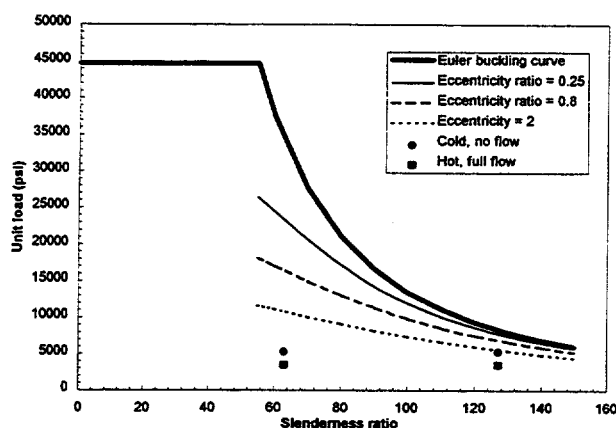


Figure 13. Extremes of F/A operating points compared to secant equation solved at eccentricity extremes.

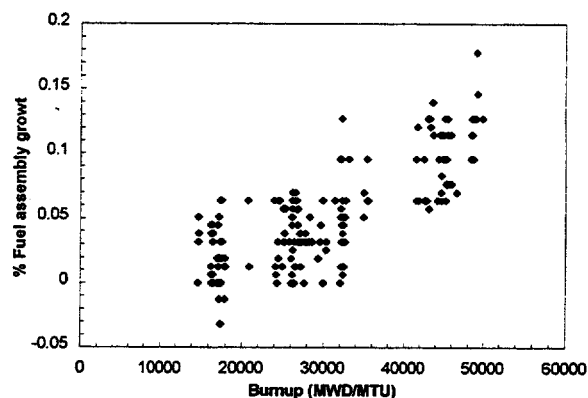


Figure 14. Fuel assembly growth measured during July and Cycle 6 offload.

Cycle 6, Standard XL and XLR F/As with longer total operating time were the most likely to have incomplete RCCA insertion implying a thermal creep-based (most likely) or fast fluence-based dependency [Hoppes 1996].

The secant equation, as presented in Section VI, can be solved for unit load as a function of slenderness ratio based on different eccentricity ratios using the physical properties of the guide tubes (i.e. I , E , σ_y). Interesting eccentricity ratios would be; the typical design value of 0.25; the maximum available in-core envelope (twice the 0.04 in grid-to-grid clearance; and the value obtained when e is based upon the maximum measured values for bow between grids (about 0.2 in) thereby producing a family of curves between expected design and the as-found (measured) worst-case. Assuming no additional bending from fuel rod thermal strains, fuel rod irradiation growth strains or interaction with adjacent F/As, the guide tube operating stress extremes would occur at hot full flow (minimum) and cold no flow (maximum) [Cloninger 1996]. The operating stress extremes can then be mapped out on the unit load-slenderness ratio plane with secant equation solutions to observe the guide tube approach to failure under the assumptions of a guided restraint boundary condition and unguided restraint boundary condition (Figure 13). As shown on the map, the guide tube is operated close to the buckling limit at the cold, no flow condition when the boundary condition is assumed to be an unguided restraint and the F/A is new ($e = 0.25$, cold no flow). If creep is assumed up to the measured limit of bow (i.e. 0.2 in), the secant curves then move closer to the operating stress and the operating stress almost crosses over the maximum measured eccentricity curve. On the other hand, the F/A probably can't actually bend as far as the unconstrained bow measurement would indicate because of interference at 0.04 in with adjacent F/As in the assembled core. Also, the boundary condition begins to change from unguided to guided restraint (i.e. the slenderness ratio decreases) as adjacent F/As are contacted. The plug gauge measurements taken on affected F/As indicate that plastic deformation has occurred in the dashpot region thereby implying that either creep resistance is insufficient to prevent excessive bowing or possibly that the combination of applied bending loads and creep eccentricity cause the unit load to be exceeded (by the mechanism of lowering the secant curve as shown), leading to yield buckling. In summary, the F/A design is very interesting when analyzed in the assembled core in that if the unit load is exceeded, it will exhibit column buckling behavior until the grids interact with another F/A on either side. Once another F/A grid is contacted and depending upon that F/A's compliance to lateral loads, the boundary condition changes to guided support and the guide tubes will act like compression members with the yield strength at approximately 45,000 psi, substantially above the applied normal stress, thereby limiting further distortion.

Utility length measurements (Figure 14) show that F/As in Cycle 6 experienced normal irradiation growth in the range of approximately 0.18% as measured by the Refueling Machine Z-Tape. Again due to Vendor request, the Vendor length

Table 2. Ranking of F/As by Burnup and Operating Time.							
Ranked by Burnup (GWD/MTU)				Ranked by operating time			
Cycle 6 Burnup (GWD/MTU)	#F/As with complete insertion	#F/As with incomplete insertion	#F/As	Total operating time (efpd)	#F/As with complete insertion	#F/As with incomplete insertion	#F/As
>48	5	7	12	1232	22	10	32
45-48	7	1	8	1001	2	1	3
43-45	7	1	8	951	2	0	2
38-43	0	0	0	742	12	0	12
35-38	0	0	0	388	8	0	8
32-35	10	2	12				
<32	17	0	17				

measurement data could not be included in here. However, the Utility has correlated the Refueling Machine Z-Tape measurement to the Vendor length measurement (the correlation data has been provided to the Vendor by the Utility). The correlation is very good although the Refueling Machine Z-Tape has a consistent bias (approximately -0.03%) and slightly higher slope (1.03%/%). The small bias differences can be easily taken out of the Z-Tape measurement using the existing data base for the correction. The small amount of F/A growth measured should not result in excessive top nozzle spring forces and should be well within design expectations.

RCCA dynamics analysis is compared to measured rod drop data for two different F/As that had incomplete insertion and normal insertions in Figures 15 and 16. These data are typical of the extremes in rod drop behavior. Clearly, the RCCA dynamics are very similar to the cases where sliding friction (guide tube bow) is modeled as opposed to increasing viscosity (that is RCCA swelling). An interesting behavior observed during testing is that some RCCAs took several minutes to go fully down as observed on DRPI indication (unfortunately, the data acquisition system can only take a few seconds of data). This behavior is partially explained by the force balance equation whereby for guide tube

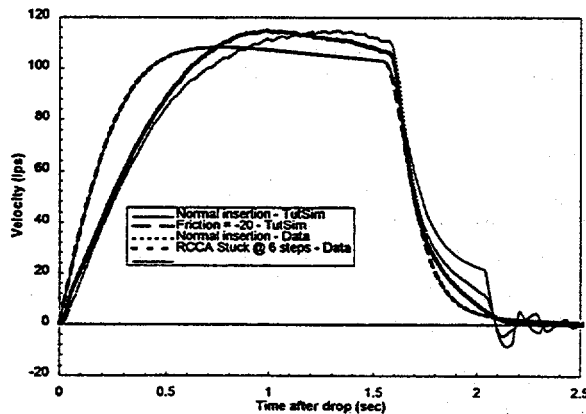


Figure 15. RCCA force balance using different sliding friction compared to measurements.

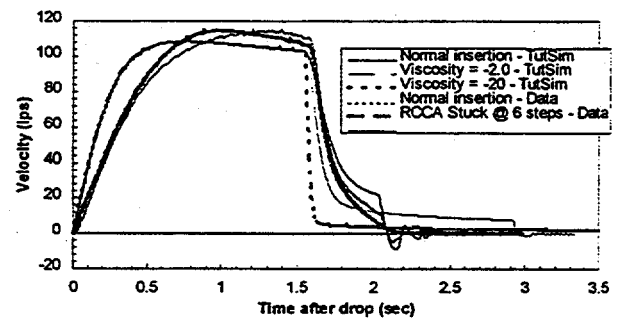


Figure 16. RCCA force balance using different viscous coefficients compared to measurements.

deflections less than that required to stop the RCCA, the RCCA velocity in the dash pot slows to lower and lower values in a continuous manner with increasing deflections until sufficient deflection is reached to stop the RCCA and it comes more suddenly to a stop. As opposed to the simplified analysis described herein, a more complete force balance analysis would include the effect of static friction. With static friction present, one would expect that at velocities low enough to take several minutes to travel 2.25 in, static friction would have taken over. However, because the F/A is continuously vibrated by coolant flow, it is unlikely that static friction can take over. RCCA dynamics analysis results for several intermediate guide tube deflections are shown in Figure 17.

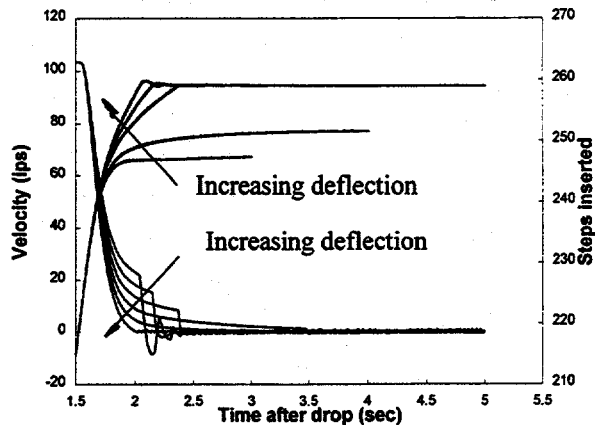


Figure 17. Typical force balance model results for intermediate bends.

VII. Conclusions

STPEGS experienced incomplete RCCA insertions in 11 F/As inserted in Unit 1, Cycle 6. The F/As had burnups in a range of between approximately 32 GWD/MTU to 50 GWD/MTU. When the simplified analysis methods presented and described herein are compared to the measurements taken, the primary hypothesis of guide tube bow is substantially supported.

The STPEGS F/As are designed such that, when assembled in the reactor core, they will behave structurally like a series of stacked columns. Regardless of the reason for initial guide tube deformation in F/As exhibiting bow, they should have no further distortion or significantly less distortion once they bow to the point of contacting adjacent F/As (depending on compliance of adjacent F/As) because the guide tube boundary condition changes from an unguided restraint to a guided restraint thereby changing the guide tube from a column to a compression member.

Based upon STPEGS experience and analysis, F/A bow measurements (bow and plug gauging considered together) are time consuming, increase the risk for the spread of contamination, require moving F/As, and are not of sufficient accuracy to support development of analytical methods for F/A structural performance. Based upon RCCA dynamics analysis, the error of the bow measurement ($1/8$ in) is on the order of the required bow measurement accuracy.

Bow measurements and core reload experience show that F/As have sufficient bow to contact adjacent F/As in the assembled core. Such contact will cause additional bending loads on F/As.

Comparison of Refueling Machine Z-Tape length measurements with the Vendor length measurement method shows that the STPEGS Unit 1 Z-Tape measurement is capable of measuring F/A growth accurately. The Refueling Machine Z-Tape method requires effectively no additional time to perform and results in no additional F/A moves when done in conjunction with core off load. Additionally, with the Z-Tape method, the entire core can be sampled whereas a sample size that large would be impractical using the Vendor method.

VIII. Recommendations

An XL F/A structural performance model based upon existing finite element methods should be developed and verified. The F/A structural model should include the ability to model influence of: thermal creep; irradiation damage (strength and elastic modulus) and growth; thermal strain; lateral constraints and interaction from adjacent F/As in the assembled core (i.e. grid-to-grid interaction); coolant flow lifting force; coolant flow-induced bending moments; grid compliance with guide tube bending; yield buckling; and fuel rod bending moments resulting from thermal and irradiation growth.

Bow measurement accuracy should be increased such that redesigned F/As can be quickly and easily tested for bow without having to move the F/As. This process could begin by qualifying Bear's method on irradiated fuel that has been well characterized with other tests.

Prototypical experiments should be performed on thimble tubes with compressive and bending loads to support constitutive equations in F/A structural models. It may be possible to extend special test methods [Wilshire 1994] to reduce the time required to get necessary creep data.

The Refueling Machine Z-Tape should be used during core offload to measure F/A growth in the future.

Plug gauge dimensions should be selected such that at least one gauge would be capable of detecting guide tube radii in ranges that would indicate yield strength exceeded in both sections of the guide tube (dash pot and thimble tube).

A standardized, mechanistic RCCA drop model should be developed and verified with plant data from different reactor designs. The model should include the effect of pressurization ahead of the RCCA tip as well as flow divisions out of the metering orifice and out of the flow holes above the dash pot.

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ROOT CAUSE OF INCOMPLETE CONTROL ROD INSERTIONS AT WESTINGHOUSE REACTORS

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ABSTRACT

Within the past year, incomplete RCCA insertions have been observed on high burnup fuel assemblies at two Westinghouse PWRs. Initial tests at the Wolf Creek site indicated that the direct cause of the incomplete insertions observed at Wolf Creek was excessive fuel assembly thimble tube distortion. Westinghouse committed to the NRC to perform a root cause analysis by the end of August, 1996.

The root cause analysis process used by Westinghouse included testing at ten sites to obtain drag, growth and other characteristics of high burnup fuel assemblies. It also included testing at the Westinghouse hot cell of two of the Wolf Creek incomplete insertion assemblies. A mechanical model was developed to calculate the response of fuel assemblies when subjected to compressive loads. Detailed manufacturing reviews were conducted to determine if this was a manufacturing related issue. In addition, a review of available worldwide experience was performed. Based on the above, it was concluded that the thimble tube distortion observed on the Wolf Creek incomplete insertion assemblies was caused by unusual fuel assembly growth over and above what would typically be expected as a result of irradiation exposure. It was determined that the unusual growth component is a combination of growth due to oxide accumulation and accelerated growth, and would only be expected in high temperature plants on fuel assemblies that see long residence times and high power duties.

Root Cause of Incomplete Control Rod Insertions at Westinghouse Reactors

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Westinghouse Commercial Nuclear Fuel Division

1.0 Introduction/ History of Events

An RCCA insertion anomaly was experienced at Wolf Creek in January 1996. During a reactor trip, five RCCAs failed to fully insert. Wolf Creek performed cold drop tests after the anomaly and three additional RCCAs did not fully insert.

Since Wolf Creek was approximately one month from EOL shutdown, the utility decided to remain shutdown and go into their refueling outage.

A similar incident had occurred at South Texas Unit 1 in December, 1995, when during a reactor trip, four RCCAs failed to fully insert. Subsequent to a review of the situation with the NRC, the unit returned to power with an agreement to conduct an RCCA trip/operability test in March, 1996. These RCCA tests resulted in seven RCCAs failing to fully insert. Subsequent to satisfying technical specifications and safety evaluation limits, the unit returned to power until May, when it shutdown. In accordance with NRC Bulletin 96-01, RCCAs were tripped and eleven RCCAs failed to fully insert. A calendar of events is shown below.

Calendar of Events

South Texas Event	12/95
Wolf Creek Event	01/96
NRC Contacted WOG/WOG Responded to NRC Questions	02/96
Westinghouse/WOG/NRC Meeting	02/96
Bulletin 96-01 Issued	03/96
NRC/Westinghouse/WOG Meeting	03/96
Commitment to determine Susceptible Fuel 06/15/96	
Commitment to determine Root Cause 08/31/96	
Westinghouse/WOG/NRC Meeting	05/96
Westinghouse Meeting with NRC on Susceptible Fuel	06/96
WOG/NRC Meeting	08/96
Westinghouse Root Cause Meeting with NRC	9/9/96

2.0 Summary of Plant Trip Information

Figure 2.1 provides a graphical representation of plant trip information for full and incomplete RCCA insertion for 50 of the 51 Westinghouse designed domestic plants (the remaining plant, Watts Bar, is not included since its fuel assembly burnup is below 27,500 MWD/MTU). This figure has been developed based on information provided by

the utilities in response to a request from the Westinghouse Owners Group (WOG). The data include information from both Westinghouse and non-Westinghouse supplied fuel of 12 foot, 10 foot, and 14 foot designs and consists of the results of beginning of cycle and end of cycle rod drop tests, in cycle rod drop tests, and RCCA insertion observations during reactor trips. The table below provides a summary of this figure with information relative to the fuel assemblies at Wolf Creek and South Texas Unit 1 incomplete RCCA insertions.

BU (GWD/MTU) RANGE UNDER RCCA's	NUMBER FAs SHOWING FULL INSERTION	NUMBER FAs SHOWING INCOMPLETE INSERTION	NUMBER FAs SHOWING INCOMPLETE INSERTION
		(WOLF CREEK)	(SOUTH TEXAS UNIT 1)
27.5 - 32.5	1593	0	1
32.5 - 37.5	1203	0	1
37.5 - 42.5	988	0	0
42.5 - 47.5	464	0	7
47.5 - 52.5	90	5	2
52.5 - 57.5	18	0	0
57.5 - 62.5	1	0	0

Summary of Plant Trip Information

Data from 50 Plants (Westinghouse & Non-Westinghouse Fuel)

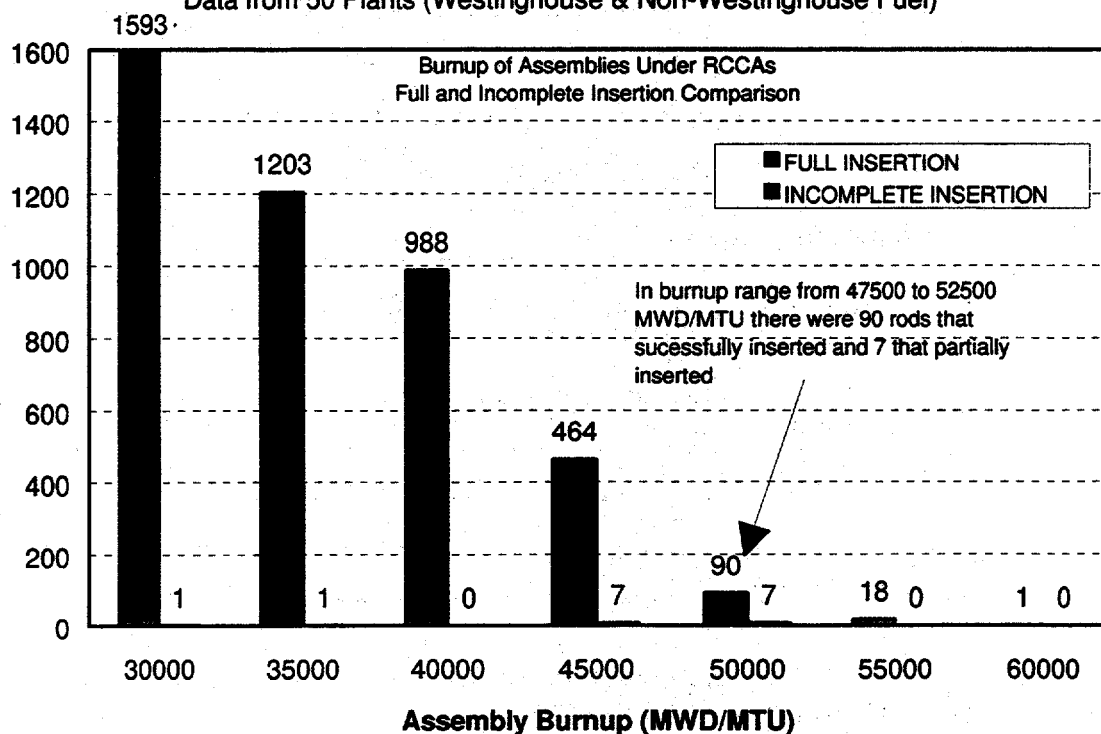


FIGURE 2.1

As indicated above, the only instances of incomplete RCCA insertion below assembly burnups of 47,500 MWD/MTU are associated with South Texas Unit 1, a plant utilizing a 14 foot fuel design. Additionally, with fuel of the 12 foot and 10 foot designs, there have been no incomplete RCCA insertions in fuel assemblies with assembly burnups below approximately 49,000 MWD/MTU.

3.0 Root Cause Process

Several activities were initiated to determine the root cause of the observed incomplete RCCA insertions. These activities are shown pictorially in Figure 3.1. The objective/purpose of these activities were as follows:

Plant Trip History Data

Determine the extent of the problem for all domestic plants that had experienced trips in the last 3-5 year period. The results were discussed in the previous section. The conclusion was that no incomplete insertions have been observed except at Wolf Creek and South Texas 1.

Detailed Manufacturing Review

Determine whether the thimble tube materials used in Wolf Creek and South Texas were unusual in any respects with regard to material specifications or process changes. Results showed no abnormalities in process or basic material properties.

Plant Operations and Fuel Management Review

Identify if there were any unique or unusual chemistry, fuel management, or core operating conditions which might suggest a cause. Both plants operated within the chemistry specifications. However, both plants have high operating temperatures and Wolf Creek (Region H) appeared to operate with a somewhat unusual power history (3 cycle operation with high power in the second and third cycle of operation).

Review of Available Worldwide Experience

Determine in a similar but less detailed fashion whether similar problems have been experienced in non-domestic plants. There are a few European plants which have experienced incomplete RCCA insertions which appear to have the same symptoms as Wolf Creek and South Texas and therefore probably have a similar root cause. Information has and continues to be gathered on these plants and incorporated in the process to determine common root causes.

Westinghouse Testing at Plant Sites

Develop and implement a detailed testing program to gather information at ten plant sites (8, excluding Wolf Creek and South Texas Unit 1). The plants and particular fuel assemblies tested were identified to cover the range of most all Westinghouse designs as well as bracket the time history of thimble tube fabrication at Wolf Creek (see Figure 3.2).

The results suggested that Wolf Creek Region H was indeed unique in terms of fuel assembly growth, particularly the fuel assemblies that experienced incomplete insertion. Wolf Creek and South Texas also displayed RCCA drag characteristics which were significantly larger than other plants tested.

Zircaloy Material Property Review

Perform a comprehensive review of all known Zircaloy properties information. Experts outside Westinghouse were also used in this process. In addition, unirradiated material tests were performed to augment existing information on the effects of oxide and hydrogen on fuel assembly guide thimble tubing. Based on the above, the Westinghouse

growth model was modified to account for these time and temperature effects and incorporated in an overall mechanical model.

Hot Cell Measurements

Determine the dimensional and material conditions/characteristics in irradiated Wolf Creek fuel assemblies to obtain basic information and insight into detailed thimble tube behavior. Fuel rods from two assemblies (H50 and H38) that had experienced incomplete insertion were removed, the skeletons sectioned and sent to the Westinghouse STC hot cell facility for detailed examination. Key information gathered was growth as a function of elevation, oxide/hydrogen as a function of elevation and dimensional characteristics of thimble tubes (ovality, bow, diameter).

Detailed Mechanical Model

Develop a mechanical model based on prior experience and basic knowledge of fuel mechanical design criteria and incorporate all the information developed on growth mechanisms. The purpose of the model was to understand the interactions of various mechanisms, interpret test results and evaluate future design changes. The comparisons between the model and test results were reasonably good considering the complexity of the problems that were modeled.

The process and testing logic used to determine the root cause are shown in Figure 3.3.

Figure 3.1: Root Cause Determination Process

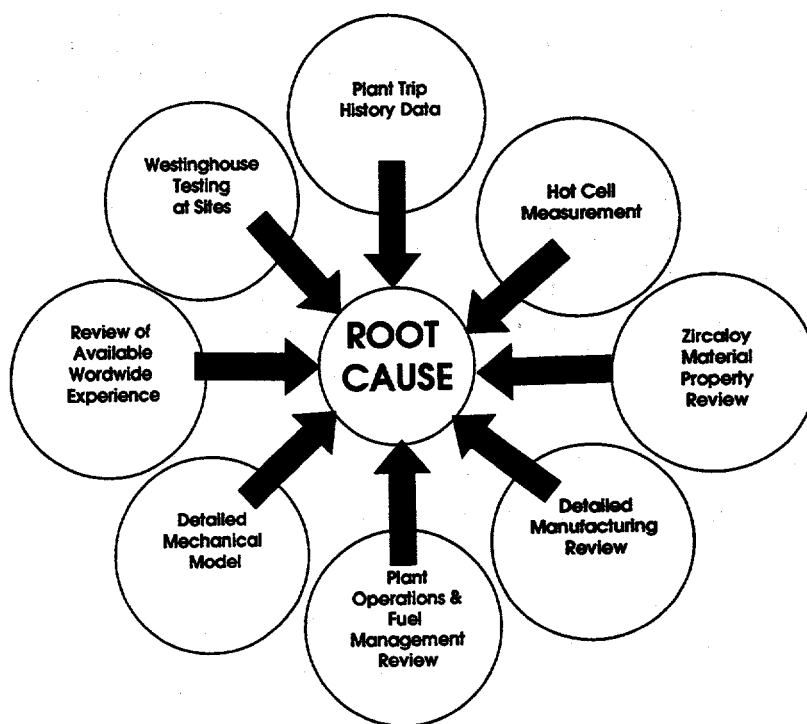


Figure 3.2: Program Status Overview

Isolation of the Population that may be Susceptible

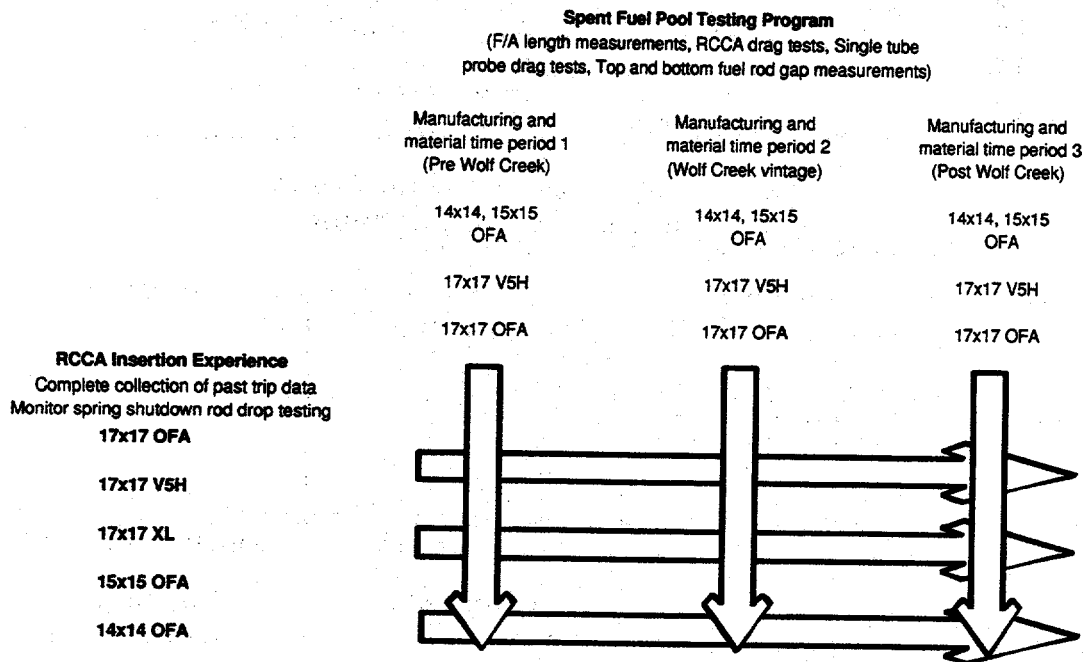
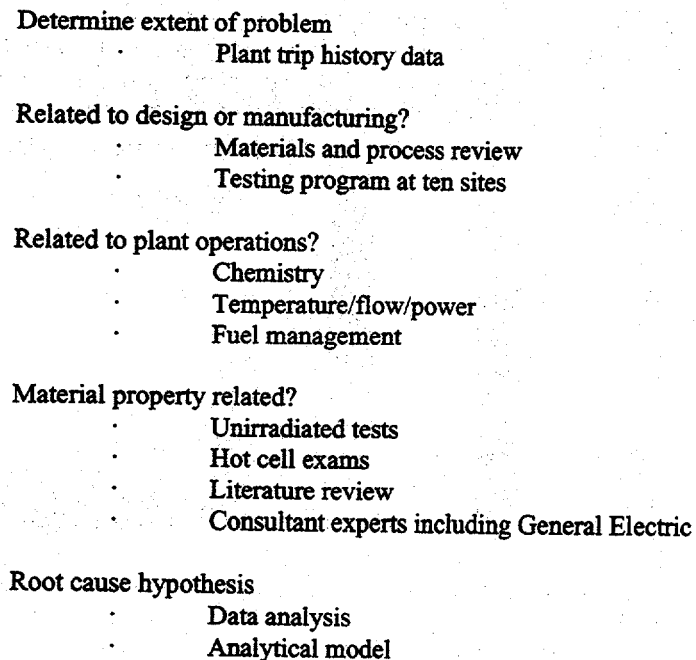


Figure 3.3: Process & testing logic used to determine root cause



4.0 Site Test Program/Logic

As previously discussed, the purpose of the Site Testing Program was to determine the susceptibility of various designs to the incomplete insertion anomaly and to gain additional information on any manufacturing effects/contributions by sampling assemblies which had thimble tubes manufactured before, during, and after the time period in which the thimble tubes for Wolf Creek were manufactured (Figure 3.2).

Table 4.1 shows the plants included in the program, their design type and the nature of the tests performed.

Table 4.1: Completed Site Testing Programs

PLANT	FUEL TYPE	VISUALS	DRAG TESTING	GROWTH	PROBE	BORESCOPE	FA BOW
Wolf Creek	17x17 V5H	√	√	√	√	√	
	17x17 w/IFM	√	√	√	√		√
	17x17 STD	√	√	√			
Millstone 3	17x17 V5H w/IFM	√	√	√	√		
South Texas	17x17 XL	√	√	√	√	√	√
Point Beach	14x14 OFA	√	√	√	√		
Surry	15x15 OFA	√	√	√	√		
VC Summer	17x17 OFA w/IFM	√	√	√	√		
Sequoyah	17x17 V5H	√	√		√		
Diablo Canyon	17x17 OFA w/IFM	√		√	√		
North Anna	17x17 V5H		√	√	√		
Vogtle	17x17 OFA w/IFM	√	√	√	√		

5.0 Susceptibility Conclusion

Based on plant trip information, site test results, observations of growth data and mechanical model results, the following conclusions regarding susceptibility to this anomaly can be drawn:

- Thimble tubes in plants manufactured from the same lot and having similar burnups as Wolf Creek did not show unusual growth, confirming that this anomaly is not manufacturing related. Therefore, thimble tube manufacturing period does not affect susceptibility to incomplete insertion.
- Comparing the drag measurements versus fluence and the drag of Wolf Creek assemblies which experienced incomplete insertion, it can be concluded that 12' assemblies with burnups less than 40 GWD/MTU are not susceptible to incomplete insertion.
- Fuel assemblies with IFMs are not susceptible to incomplete insertion.

6.0 Summary of Key Conclusions Leading to the Determination of Root Cause

The following conclusions were drawn based on the results of the detailed test programs and analysis:

- 1 The trip history data from the plants demonstrated that there were a significant number of assemblies operating at high burnup levels under control rod locations without showing insertion problems.
2. Review of the available worldwide experience indicated that in almost all cases that RCCA insertion problems have been reported (other than those cases that have been attributed to debris or Control Rod drive mechanism problems), the causes were related to excessive compressive loads on the fuel assembly guide thimble tubes.
3. The detailed manufacturing review indicated that this problem was not related to a manufacturing anomaly. Additionally, thimble tubes in other plants from the same lot and similar burnups as Wolf Creek did not show unusual growth, thereby confirming that this was not a manufacturing related issue.
4. A detailed review of plant operations and fuel management showed that the Wolf Creek assemblies that had exhibited incomplete insertion were somewhat unique in their power history behavior. These assemblies operated for three cycles with relatively high power in the second and third cycles in a high temperature environment.
5. The results of the growth measurements showed that fuel assemblies that showed high growth were only seen in the high temperature plants. However, high temperature seemed to be a necessary but not a sufficient condition for high growth. For high temperature plants, specific power histories seemed to influence growth, specially those that had long residence times and high power in the later stages of operation.
6. The hot cell results from the two incomplete insertion assemblies showed that a significant portion of the growth was related to oxide formation. As well known, oxide formation is a strong function of temperature and residence time. Also, the remainder of the growth was greater than what would be expected for normal saturation growth. This component was attributed to accelerated growth, a phenomenon that is reported in the literature and is shown to be very temperature sensitive.
7. Data from the literature on Zircaloy growth suggested that depending on the temperature and fluence level, accelerated growth could occur. This growth would proceed after an incubation period. The point at which

this growth would initiate, as well as the slope of the growth versus fluence curve was very temperature dependent. It is postulated that the high temperature in the later cycles of operation (consistent with the incubation period theory) would exacerbate the accelerated growth mechanism.

8. The detailed mechanical model predicted the growth differences between the Wolf Creek incomplete insertion assemblies, the Wolf Creek complete insertion assemblies and the South Texas assemblies reasonably well. In addition, the model was able to reasonably reproduce the span dependent bow measurements that were obtained from the hot cell on the Wolf Creek incomplete insertion assemblies. This model used a free growth correlation with the accelerated growth component normalized to the actual growth measurement from one of the Wolf Creek incomplete insertion assemblies.

7.0 Root Cause Conclusions

Based on the above combination of reviews, testing and analysis, the root cause conclusions were determined to be as follows:

- The incomplete RCCA insertions observed at Wolf Creek have been caused by excessive compressive loads on the fuel assembly guide thimble tubes leading to excessive thimble tube distortion.
- For Wolf Creek, the increased compressive load was caused by unusual fuel assembly growth over and above what would normally be expected as a result of irradiation exposure.
- The unusual growth component is a combination of growth due to oxide accumulation and accelerated growth, both of which are temperature sensitive.
- The unusual growth is observed only in high temperature plants on those high burnup fuel assemblies that have certain types of power histories.
- The apparent cause of the incomplete Rod Cluster Control Assembly insertion at the South Texas Project is fuel assembly thimble tube distortion resulting from high, in-vessel, compressive loading imparted on the assembly skeleton. The problematic distortion is limited to the assembly dashpot area of the thimble tubes which prevents complete control rod insertion. Westinghouse will continue to work with the South Texas Project to identify the root cause, along with short and long term corrective actions. The final conclusions of this control rod insertion anomaly will be documented in future Nuclear Regulatory Commission Bulletin 96-001 correspondence.

It should be noted that these conclusions are based on Westinghouse models and data taken on Westinghouse fuel assemblies. They are not applicable for non-Westinghouse designs.

REGULATORY PERSPECTIVE ON INCOMPLETE CONTROL ROD INSERTIONS

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ABSTRACT

The incomplete control rod insertions experienced at South Texas Unit 1 and Wolf Creek are of safety concern to the NRC staff because they represent potential precursors to loss of shutdown margin. Even before it was determined if these events were caused by the control rods or by the fuel there was an apparent correlation of the problem with high burnup fuel. It was determined that there was also a correlation between high burnup and high drag forces as well as with rod drop time histories and lack of rod recoil. The NRC staff initial actions were aimed at getting a perspective on the magnitude of the problem as far as the number of plants and the amount of fuel that could be involved, as well as the safety significance in terms of shutdown margin.

As tests have been performed and data has been analyzed our focus has shifted more toward understanding the problem and the ways to eliminate it. At this time the staff's understanding of the phenomena is that it was a combination of factors including burnup, power history and temperature. The problem appears to be very sensitive to these factors, the interaction of which is not clearly understood. The model developed by Westinghouse provides a possible explanation but there is not sufficient data to establish confidence levels and sensitivity studies involving the key parameters have not been done. While several fixes to the problem have been discussed, no definitive fixes have been proposed. Without complete understanding of the phenomena, or fixes that clearly eliminate the problem the safety concern remains. The safety significance depends on the amount of shutdown margin lost due to incomplete insertion of the control rods. Were the control rods to stick high in the core, the reactor could not be shutdown by the control rods and other means such as emergency boration would be required.

REGULATORY PERSPECTIVE ON INCOMPLETE CONTROL ROD INSERTIONS

The incomplete control rod insertions experienced at South Texas Project Unit 1 and Wolf Creek are of safety concern to the staff of the Nuclear Regulatory Commission (NRC) because they represent potential precursors to loss of shutdown capability. It is this safety concern that prompted immediate action by the NRC staff. This paper contains a brief description of the incomplete control rod insertion events and of the NRC staff actions. Also described are results of subsequent tests and their significance and future NRC plans pertaining to this issue.

Description of Events

South Texas Project

On December 18, 1995, with South Texas Unit 1 at 100 percent power, a pilot wire monitoring relay actuation caused a main transformer lockout, which resulted in a turbine trip and a reactor trip. While verifying that control rods had inserted fully after the trip, operators noted that the rod bottom lights of three control rod assemblies were not lit; the digital rod position indication for each rod indicated six steps withdrawn. A step is equivalent to 1.59 cm [5/8 inch], and the top of the dashpot begins at 38 steps. One rod drifted into the fully inserted rod bottom position within 1 hour, and the other two rods were manually inserted later. During subsequent testing of all control rods in the affected banks, the rod position indication for the same three locations, as well as a new location, indicated six steps withdrawn. As compared to prior rod drop testing, no significant differences in rod drop times were noted before reaching the upper dashpot area for any of the control rods. Within 1 hour after the rod drop tests, two of the rods drifted to the rod bottom position and the other two were manually inserted. All four control rods were located in XLR fuel assemblies which were in their third cycle, with burnup greater than 42,880 megawatt days per metric ton uranium (MWD/MTU).

Wolf Creek Plant

On January 30, 1996, after a manual scram from 80 percent power, five control rod assemblies at the Wolf Creek plant failed to insert fully. Two rods remained at 6 steps withdrawn, two at 12 steps, and one at 18 steps. At Wolf Creek, a step is equivalent to 1.59 cm [5/8 inch] and the top of the dashpot begins at approximately 30 steps. Three of the affected rods drifted to the fully inserted position within 20 minutes, one within 60 minutes, and the last one within 78 minutes. The results also indicate that there was some slowing down of affected rods before they reached the dashpot. After the scram, the licensee initiated emergency boration because all rods did not insert fully. During subsequent cold rod drop tests, the same five rods, plus an additional three rods, failed to fully insert. All of the affected rods were in 17x17 VANTAGE 5H fuel assemblies, with burnup greater than 47,600 MWD/MTU.

North Anna Plant

On February 21, 1996, during the insert shuffle in preparation for loading North Anna Unit 1, Cycle 12, two new control rod assemblies could not be removed with normal operation of the handling tool from the fuel assemblies in the spent fuel pool in which they were temporarily stored. The control rod assemblies were removed using the rod assembly handling tool in conjunction with the bridge crane hoist. The two affected fuel assemblies were VANTAGE 5H assemblies, which had achieved burnups of 47,782 MWD/MTU and 49,613 MWD/MTU during two cycles of irradiation.

At both South Texas units, a 14-foot active fuel length core design is used. Several differences between the standard 12-foot active fuel design and the 14-foot design are as follows: the 14-foot fuel design is approximately 76.2 cm [30 inches] longer than the standard fuel assembly design, it has 10 mid grids compared to 8, and the dashpot region is 25.4 cm [10 inches] longer and comprises a double dashpot. The control rod radial clearances above and in the dashpot region of the 14-foot fuel assembly are similar to those of the standard design. The South Texas core contained three different 17x17 fuel types--Standard XL, Standard XLR, and VANTAGE 5H--all of which are designed and fabricated by Westinghouse. The core contained 57 control rods, all of which are silver-indium-cadmium rods. The four affected rods were found in twice-burned Standard XLR fuel assemblies.

During subsequent testing, the rod drop traces revealed no significant change in dashpot entry time; however, the affected rods did not show recoil on the rod drop trace. Recoil is a dampening effect that is normally seen in the traces as a result of contact of the control rod assembly spider hub spring with the fuel assembly. The testing of similar rods in Unit 2 revealed no adverse indications. One rod did show the "no recoil" effect but inserted fully into the core.

At Wolf Creek, subsequent cold, full-flow testing of all of the control rod assemblies indicated that eight control rods, including the five control rods that did not fully insert following the reactor trip on January 30, 1996, did not fully insert when tripped. One control rod in core location H2 paused at 96 steps, stopped at 90 steps, and slowly inserted to 30 steps over the next 2 hours. The control rod was then manually inserted. The seven other affected rods stopped at various heights in the dashpot region, five of which fully inserted within 22 minutes. One of the other two drifted to the bottom within 1.5 hours: the remaining rod needed to be manually inserted. The remaining 45 rods fully inserted when dropped, although a number of them did not exhibit the expected number of recoils. Of the total 53 control rod assemblies, the assembly at core location H2 (the only rod stopping outside the dashpot region) was a hafnium control rod: the remaining were silver-indium-cadmium control rod assemblies. However, subsequent inspection of the hafnium rod did not indicate any adverse dimensional change. The licensee retested all rods that stuck, as well as those rods that failed to recoil more than twice, and the results were similar to the results of the previous testing.

At North Anna, the two affected control rods were removed and were inserted into a series of other fuel assemblies. No additional binding was observed. However, difficulty was

experienced when another control rod was inserted into the two affected fuel assemblies. On the basis of this result, the licensee determined that the cause of the binding was related to the fuel assemblies and not the control rods. Subsequent control rod drag testing data indicated a correlation of control rod drag force to assembly burnup and a significant increase in drag force at assembly burnups greater than 45,000 MWD/MTU.

NRC Staff Actions

These three events, as well as several similar events at foreign reactors, raised concerns about the operability of control rods in high burnup fuel assemblies. Although most of the testing has demonstrated that the control rods have reached the dashpot region of the guide tube and that adequate shutdown margin has been maintained, there have been indications of degraded rod drop times and a stuck rod well above the dashpot region. Thus, there is concern that these events may be precursors of more significant control rod binding problems in which required shutdown margins and rod drop times may be violated.

Even before it was determined whether these events were caused by the control rods or by the fuel there was an apparent correlation of the problem with high burnup fuel. It was determined that a correlation also existed between high burnup and high drag forces as well as with rod drop time histories and lack of rod recoil. Initial actions of the NRC staff were aimed at obtaining a perspective on the magnitude of the problem in regards to the number of plants and the amount of fuel that could be involved, as well as the safety significance in terms of shutdown margin.

The staff's initial actions were to activate the Westinghouse Owners Group (WOG) Regulatory Response Group and to issue Information Notice 96-12, "Control Rod Insertion Problems." The staff then met with the WOG to discuss the safety significance and the generic implications of the incomplete control rod insertion issue. At that meeting the WOG responded to a set of 14 questions from the NRC staff and discussed its future plans.

Representatives from Office of Nuclear Reactor Regulation (NRR), Office of Nuclear Regulatory Research and Region IV observed the testing at Wolf Creek and South Texas. The NRC staff was involved in the development of the test program and the analysis of the data. NRR staff members met with Westinghouse staff in Columbia, South Carolina to examine the data analysis and results from the testing at Wolf Creek. NRR staff members also witnessed hot-cell testing programs at Westinghouse facilities near Pittsburgh, Pennsylvania.

The testing at Wolf Creek included drag testing both in the reactor vessel with the upper internals in place and in the spent fuel pool. These tests were performed with the control rods in their host fuel assemblies. These tests were followed by drag testing of the control rods in a reference assembly. The results of the drag testing showed that the binding that caused incomplete control rod insertion was not caused by the control rods.

Fuel assembly tests included length and bow measurements, boroscope inspections, and single-tube probes. The results of these tests showed that the incomplete control rod insertion at Wolf Creek was caused by thimble tube distortion.

The NRC staff issued Bulletin 96-01, "Control Rod Insertion Problems" to alert all licensees of Pressurized Water Reactors (PWRs) to the failure of control rods to fully insert following a scram signal and to assess the operability of control rods, particularly in high burnup fuel. The bulletin was sent to all holders of PWR operating licenses, but a response was required only from PWR licensees of Westinghouse-designed plants.

All licensees of Westinghouse-designed plants were requested to take the following actions:

- (1) Promptly inform operators of the reactor trips and testing in which control rods did not fully insert and subsequently provide necessary training, including simulator drills, utilizing the required procedures for responding to an event in which the control rods do not fully insert upon reactor trip (e.g., boration of a prespecified amount).
- (2) Promptly determine the continued operability of control rods based on current information. As new information becomes available from plant rod drop tests and trips, licensees should consider this new information together with data already available from Wolf Creek, South Texas, North Anna, and other industry experience, and make a prompt determination of control rod operability.
- (3) Measure and evaluate at each outage of sufficient duration during calendar year 1996 (end of cycle, maintenance, etc.), the control rod drop times and rod recoil data for all control rods. If appropriate plant conditions exist where the vessel head is removed, measure and evaluate drag forces for all rodded fuel assemblies.
 - a. Rods failing to meet the rod drop time in the technical specifications shall be deemed inoperable.
 - b. Rods failing to bottom or exhibiting high drag forces shall require prompt corrective action in accordance with Appendix B to Part 50 of Title 10 of the Code of Federal Regulations (10 CFR Part 50).
- (4) For each reactor trip during calendar year 1996, verify that all control rods have promptly fully inserted (bottomed) and obtain other available information to assess the operability and any performance trend of the rods. In the event that all rods do not fully insert promptly, conduct tests to measure and evaluate rod drop times and rod recoil.

In addition responses were required as follows:

- (1) Within 30 days of the date of the bulletin, a report certifying that control rods are determined to be operable; actions taken for Requested Actions (1) and (2) above; and the plans for implementing Requested Actions (3) and (4).

- (2) Within 30 days of the date of the bulletin, a core map of rodged fuel assemblies indicating fuel type (materials, grids, spacers, guide tube inner diameter) and current and projected end of cycle burnup of each rodged assembly for the current cycle; when available, provide the same information for the next cycle.
- (3) Within 30 days after completing Requested Action (3) for each outage, a report that summarizes the data and that documents the results obtained; this is also applicable to Requested Action (4) when any abnormal rod behavior is observed.

Responses to the bulletin were due April 8, 1996.

During that same period the NRC staff met with the Combustion Engineering Owners Group (CEOG) and the Babcox & Wilcox (B&W) Owners Group to discuss the staff concerns pertaining to high burnup fuel and stuck control rods. The objectives of the meeting were to heighten industry awareness of high burnup fuel and control rod problems being experienced at some Westinghouse designed plants, and to gain an understanding of industry plans and actions in response to these problems. The staff requested that the owners groups become proactive and collect information to determine if failure of control rods to fully insert had occurred at their facilities.

The CEOG stated that it had sent out a survey to the CE plants and committed to provide a report. The NRC received that report in August 1996. The main conclusions of the report are as follows:

- (1) A comparison of the CE 14X14 and 16X16 guide tube designs with the small diameter thimble tube designs reveals a considerable lateral strength advantage for the CE designs based on shorter guide tube lengths, significantly larger ID and OD diameters and larger ratios of thickness to OD wall for both the 14X14 and 16X16 designs.
- (2) The CE design has considerable advantage relative to the small diameter thimble tube design to resist individual tube buckling induced distortions.
- (3) Review of CEA operability and insertion issues at CE PWRs revealed no problems related to either fuel assembly design or the burnup achieved by CEA host assemblies.
- (4) A review of CEA drop times showed no dependence on the design or burnup of any host fuel assembly, up to the maximum burnup associated with the tests performed.

The B&W Owners Group has been working on a similar report and committed to submit it to the NRC by the end of October 1996.

As tests have been performed and data have been analyzed, the staff's focus has shifted more toward understanding the problem and ways to eliminate it.

The staff has evaluated the bulletin responses and has continued to monitor the root cause activities of Westinghouse and the WOG. It has held weekly conference calls on the progress of the site testing, the hot cell testing, and the root cause: had several meetings with Westinghouse and the WOG: and has observed testing at the hot cell facility.

The core map data in response to the bulletin have been used to determine how much fuel of each type is in use as well as the current and expected end-of-cycle (EOC) burnup on this fuel. As plants have shut down for refueling outages, performed the testing requested by the bulletin and reported their results, the staff has analyzed the data from the rod drop testing and the drag testing.

Based on Westinghouse data and models for its fuel, Westinghouse submitted to the staff the following root cause conclusions:

- (1) The incomplete RCCA insertions observed at Wolf Creek have been caused by excessive compressive loads on the fuel assembly guide thimble tubes which led to excessive thimble tube distortion.
- (2) For Wolf Creek, the increased compressive load was caused by unusual fuel assembly growth over and above what would normally be expected as a result of irradiation exposure.
- (3) The unusual growth component is a combination of growth due to oxide accumulation and accelerated growth, both of which are temperature sensitive.
- (4) The unusual growth is observed only in high temperature plants on those high burnup fuel assemblies that have certain types of power histories.

Future Staff Actions

At this time the staff's understanding of the phenomenon is that it was a combination of factors including burnup, power history, and temperature. The problem appears to be very sensitive to these factors, the interaction of which is not clearly understood. Although the proposed root cause is a plausible explanation it is not conclusive. The model for growth due to oxide accumulation is based on a very small number of data points and appears to be extremely sensitive to temperature. This model developed by Westinghouse provides a possible explanation but data are not sufficient to establish confidence levels, and sensitivity studies involving the key parameters have not been done. Furthermore, these studies are not planned.

Without a more complete understanding of the phenomenon, remedial actions that clearly eliminate the problem cannot be defined, and the safety concern remains. The safety significance depends on the amount of shutdown margin lost because of incomplete insertion of the control rods. Were the control rods to stick at higher locations in the core, the reactor could not be shut down by the control rods, and other means such as emergency boration would be required. The regulatory requirement for the operation of the control rods is General Design Criterion (GDC) 26, "Reactivity control system redundancy

and capability," of Appendix A to 10 CFR 50 which specifies "Two independent reactivity control systems of different design principles shall be provided. One of the systems shall use control rods, preferably including a positive means for inserting the rods, shall be capable of reliably controlling reactivity changes to assure that under conditions of normal operation, including operational occurrences, and with appropriate margin for malfunctions such as stuck rods, specified acceptable fuel design limits are not exceeded."

Because of the continuing safety concern, the NRC staff is evaluating possible approaches to expedite resolution of the issue. The approaches that it is considering are:

- (1) Enhanced requirements for lead test assemblies (LTAs)
- (2) Surveillance to detect metallurgical problems
- (3) Fuel management guidelines.

Although, some previous assembly growth data gave an indication of the problem of excessive growth, this precursor was not recognized. It is important to reexamine the LTA program to avoid missing similar precursors in the future. The LTA programs to date did not give indication that a thimble tube distortion problem was likely. However, it must be noted that the LTAs were not tested under the same conditions as those under which the fuel was used. There are several considerations in this regard:

- (1) LTAs were not used in rodded locations
- (2) Burnup rate was not representative, i.e., burning an assembly to a given burnup in 5 cycles has different impacts than burning the assembly to the same level over 2 or 3 cycles
- (3) Temperatures were not representative, i.e., burning LTAs at a given temperature does not necessarily reflect behavior at a slightly higher temperature.

Thus it has become apparent that LTAs need to be tested in prototypical conditions if the results are to be meaningful for assessing potential high burnup sensitivity.

The staff is also considering the benefits of additional periodic testing requirements such as surveillance to detect metallurgical problems, or drag testing. A testing program that could assure complete control rod insertion might be a long-term solution to this control rod insertion problem.

Fuel management restrictions may also be needed to assure control rod operability since no long-term solution to the problem has been proposed by industry. It is the staff's understanding that the following parameters are of importance:

- (1) Temperature of individual assemblies
- (2) Core height
- (3) Burnup and burnup rate
- (4) Immediate Flow Mixing grids (or other stiffeners)

In the absence of an industry proposal, the staff examined the available data relevant to these parameters to see what form of fuel management guidelines might be reasonable. An example set is as follows:

If $T_{out} > 610^{\circ}\text{F}$ limit burnup on rodged fuel assemblies to:

- 35,000 MWD/MTU for assemblies without IFMs for 12 ft cores.
- 30,000 MWD/MTU for assemblies without IFMs for 14 ft cores.
- 40,000 MWD/MTU for assemblies with IFMs for 12 ft cores.
- 35,000 MWD/MTU for assemblies with IFMs for 14 ft cores.

If $T_{out} < 610^{\circ}\text{F}$ limit burnup on rodged fuel assemblies to:

- 45,000 MWD/MTU for assemblies without IFMs for 12 ft cores.
- 40,000 MWD/MTU for assemblies without IFMs for 14 ft cores.
- 50,000 MWD/MTU for assemblies with IFMs for 12 ft cores.
- 45,000 MWD/MTU for assemblies with IFMs for 14 ft cores.

These example guidelines bound the data that the NRC has to date. They reflect the sensitivity to burnup and temperature that has been observed. They also reflect the additional stiffness that should be provided by assemblies with intermediate flow mixing grids (IFMs). There is concern, however, that while the middle of assemblies with IFMs should be more resistant to bowing, the top and bottom spans of an assembly with IFMs would be even more vulnerable to bowing. Since significant bowing in the top span could stick the control rod completely out of the core, this concern would also need to be addressed by any industry proposal in addition to the four parameters listed above.

Although the incomplete control rod insertion has been experienced in only a limited number of fuel designs, the staff is not convinced that other designs are not susceptible. Fuel designs involving relatively small diameter thimble tubes, high exit temperatures, high burnup rate and high burnup are apparently particularly susceptible to this phenomenon. As vendors attempt to manufacture fuel designs that are compatible with other vendors fuel, susceptibility to thimble tube bowing and susceptibility to control rod sticking may result. Consequently NRC staff is expecting all vendors and owners groups to address this problem. The staff is expecting the B&W Owners Group report by the end of the month. The staff will review the report and consider the need for follow-up actions.

Based on the differences in the fuel design, and the understanding of the root cause phenomenon, the staff is in general agreement with the conclusions presented in the CE Owners Group report; mainly that the fuel designs currently used in CE plants do not appear to be susceptible to this phenomenon.

Establishing Seismic Design Criteria to Achieve an Acceptable Seismic Margin

by

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Abstract

In order to develop a risk based seismic design criteria the following four issues must be addressed:

1. What target annual probability of seismic induced unacceptable performance is acceptable?
2. What minimum seismic margin is acceptable?
3. Given the decisions made under Issues 1 and 2, at what annual frequency of exceedance should the Safe Shutdown Earthquake ground motion be defined?
4. What seismic design criteria should be established to reasonably achieve the seismic margin defined under Issue 2?

The first issue is purely a policy decision and is not addressed in this paper. Each of the other three issues are addressed.

Issues 2 and 3 are integrally tied together so that a very large number of possible combinations of responses to these two issues can be used to achieve the target goal defined under Issue 1. Section 2 lays out a combined approach to these two issues and presents three potentially attractive combined resolutions of these two issues which reasonably achieves the target goal. The remainder of the paper discusses an approach which can be used to develop seismic design criteria aimed at achieving the desired seismic margin defined in resolution of Issue 2. Suggestions for revising existing seismic design criteria to more consistently achieve the desired seismic margin are presented.

1. Introduction

The U.S. Nuclear Regulatory Commission (NRC) has requested that all existing nuclear power plants define their plant seismic margins in terms of the High-Confidence-Low-Probability-of-Failure (HCLPF) seismic capacity (Ref. 1). The seismic margin for the design basis Safe-Shutdown-Earthquake (SSE) can then be defined by the ratio between the HCLPF and SSE ground motion levels. In addition, the NRC is moving toward defining the SSE ground motion response spectrum with a specified annual frequency of exceedance (Ref. 2).

Two obvious questions are:

1. What seismic margin (HCLPF/SSE ratio) is sufficient to achieve a specified seismic performance goal defined in terms of a target maximum annual probability of unacceptable seismic performance, given that the SSE is defined in terms of an annual frequency of exceedance?
2. What deterministic seismic design/evaluation criteria will reasonably achieve this desired seismic margin?

This paper lays out a framework for addressing these two questions. Specifically, the paper defines the relationship that exists between a specified seismic margin and the ratio R_p , where:

$$R_p = \frac{H_{SSE}}{P_f} \quad (1)$$

in which H_{SSE} is the annual frequency of exceeding the SSE (hereafter called SSE exceedance frequency) and P_f is the annual probability of unacceptable seismic performance (hereafter called failure probability). Next, the paper lays out guidance for establishing deterministic seismic design criteria which will roughly achieve the desired seismic margin. It will be suggested that the seismic demand (response) analysis should be performed with sufficient conservatism to achieve roughly an 84% non-exceedance probability on the computed demand. Next, the seismic capacity should be established sufficiently conservative to achieve a specified factor of safety on the 1% conditional probability of failure capacity given the condition that the computed demand actually occurs. The required factor of safety will be shown to be approximately equal to the desired HCLPF seismic margin.

A risk based seismic design criterion can be established by:

1. specifying a target permissible seismic-induced failure probability P_f for seismic safety significant structures, systems, and components (hereafter called a component).
2. specifying seismic response and capacity criteria aimed at achieving a specified desired HCLPF seismic margin
3. determining the ratio R_p consistent with the specified HCLPF seismic margin from Table 1 as will be discussed in Section 2
4. specifying the SSE ground motion level at an exceedance frequency H_{SSE} given by:

$$H_{SSE} = R_p P_f \quad (2)$$

Following is the approach, the U.S. Department of Energy (DOE) has adopted a risk based seismic design and evaluation criterion approach in DOE-STD-1020-94 (Refs. 3 and 4) for DOE facilities.

2. Relationship Between Seismic Margin and Probability Ratio R_p

Figure 1 presents two representative probabilistic seismic hazard curves expressed in terms of mean annual frequency of exceedance versus peak ground acceleration. Over any ten-fold difference in exceedance frequencies such hazard curves may be approximated by:

$$H_{(a)} = K_1 a^{-K_H} \quad (3)$$

where $H_{(a)}$ is the annual frequency of exceedance of ground motion level "a", K_1 is an appropriate constant, and K_H is a slope parameter defined by:

$$K_H = \frac{1}{\log(A_R)} \quad (4)$$

in which A_R is the ratio of ground motions corresponding to a ten-fold reduction in exceedance frequency.

In order to compute the probability ratio R_p corresponding to any specified seismic design/evaluation criteria, one must also define a mean seismic fragility curve for a component resulting from the usage of these seismic criteria. This mean seismic fragility curve describes the conditional probability of an unacceptable performance versus the ground motion level. This mean fragility curve is most often defined as being lognormally distributed and is expressed in terms of two parameters: a median capacity level and a composite logarithmic standard deviation β (see Ref. 2 for further amplification). The logarithmic standard deviation β will generally lie within the range of 0.3 to 0.5 for structures and equipment mounted at ground level. For equipment mounted high in structures, β will generally lie within the range of 0.4 to 0.6.

For any component, mean probability P_F of unacceptable performances is obtained by convolution of the seismic hazard and fragility curves expressed by:

$$P_F = -\int_0^{+\infty} \left(\frac{dH(a)}{da} \right) P_{F/a} da \quad (5)$$

where $P_{F/a}$ is the conditional probability of failure given the ground motion level "a" which is defined by the mean fragility curve.

Approximating the seismic hazard curve between the SSE exceedance frequency H_{SSE} and the failure probability P_F by Eqn. (3), Ref. 4 shows that:

$$R_p = \frac{H_{SSE}}{P_f} = (F_p)^{K_H} e^f \quad (6)$$

$$f = X_p K_H \beta - 1/2 (K_H \beta)^2 \quad (7)$$

where F_p is the factor of safety (seismic margin) between the SSE and the component seismic capacity S_p associated with the conditional failure probability $P_{F/a}$, and X_p is the standardized normal variant associated with the failure probability $P_{F/a}$. For example,

$P_{F/A}$	X_p
1%	2.326
5%	1.645

$P_{F/A}$	X_p
10%	1.282
≈16%	1.0

Initially, the HCLPF seismic capacity was defined as the level at which one had approximately 95% confidence of less than about 5% probability of failure. However, in Ref. 1 the NRC has suggested that this HCLPF seismic capacity can be approximated as the 1% composite (mean) probability of failure. With this approximation, one does not have to separate their estimates of variability into "uncertainty" and "randomness," but can work with a single composite (mean) fragility curve which defines mean probability of failure versus ground motion level. For simplicity, this composite (mean) approximation of the HCLPF capacity will be used herein. Therefore, the probability ratio R_p can be defined in terms of the HCLPF seismic margin F_H by setting $X_p = 2.326$ in Eqns. (6) and (7).

For the Central and Eastern United States (CEUS), within the mean exceedance frequency range from 10^{-4} to 10^{-5} , the A_R ratio for ground motion spectral accelerations is typically:

<u>CEUS</u>	$A_R = 2.0 \text{ to } 4.0 \text{ (5 to 10 Hz)}$
	$A_R = 3.0 \text{ to } 6.0 \text{ (1.0 Hz)}$

However, for California and other high seismic sites with seismicity dominated by close active faults with high recurrence rates associated with tectonic plate boundaries, the corresponding A_R ratio is typically:

<u>Calif.</u>	$A_R = 1.5 \text{ to } 2.0 \text{ (5 to 10 Hz)}$
	$A_R = 2.0 \text{ to } 3.0 \text{ (1 Hz)}$

Therefore, the appropriate A_R ratio to use in Eqn. (4) to define K_H is both a function of the site location and the natural frequency at which the ground motion spectral acceleration is being defined.

Table 1 presents the minimum probability ratio R_{pmin} obtained over the typical seismic fragility logarithmic standard deviation β range of 0.3 to 0.6 for various candidate HCLPF seismic margins F_H ranging from 1.0 to 1.67 as a function of the ground motion ratio A_R . It can be seen that a constant R_{pmin} ratio cannot be achieved over the full range of typical A_R ratios using any of these candidate HCLPF seismic margins.

Based both upon performing HCLPF seismic margin calculations for many components at a number of nuclear power plants and upon reviewing HCLPF seismic margin calculations performed by others, it is the author's opinion that the current Standard Review Plan (SRP) seismic criteria contained in Ref. 6 typically leads to a HCLPF seismic margin of 1.25 to 1.5 when the SRP criteria is just barely met. This opinion is bolstered by the observation reported in Ref. 7 that after anchorage, seismic-interaction, or other significant deficiencies caught as a result of a careful seismic walkdown of a plant have been corrected, seismic probabilistic risk assessments (PRA) and seismic margin studies conducted on existing nuclear power plants have reported HCLPF seismic margins of 1.25 or greater. Therefore, it is expected that establishing seismic design criteria aimed at achieving a HCLPF seismic margin F_H in the range of 1.25 to 1.5 would result, on average, in the minimum required changes to SRP seismic acceptance criteria.

2.1 Consequences on Establishing An SSE Exceedance Frequency

Strictly speaking, R_p defined by Eqn. (6) presents the ratio between the mean annual frequency of exceedance of the SSE and the mean annual probability of unacceptable performance. However, although not rigorous, this ratio also accurately approximates the ratio between the median exceedance frequency and the median probability of unacceptable performance. Therefore, so long as the seismic risk goal is defined in terms of a median failure probability, R_{pmin} from Table 1 may be used to determine the appropriate median hazard exceedance frequency H_{SSE} . Because the ground motion corresponding to a median hazard exceedance frequency has been found to be more stable when compared between various prediction procedures than the ground motion corresponding to a mean hazard exceedance frequency, the proposed D.R.G. 1032 (Ref. 2) currently defines the SSE in terms of a median exceedance frequency. A seismic risk based design criteria can be developed and defined in terms of either a mean or median risk goal. The only requirement is that both the hazard exceedance frequency and the probability of failure goal be defined in the same term (median or mean).

If the SRP deterministic seismic acceptance criterion is aimed at achieving a specified HCLPF seismic margin in the range of 1.25 to 1.5, the goal of a target minimum annual probability of unacceptable seismic performance P_F for a component cannot be universally achieved even approximately for all sites and over all natural frequencies by specifying the SSE ground response spectrum by an Uniform Hazard Spectrum (UHS) at a constant specified annual exceedance frequency H_{SSE} . The exceedance frequency H_{SSE} must be made a function of the ground motion ratio A_R appropriate for the site's spectral acceleration hazard at the natural frequency at which the spectral acceleration is being defined.

For example, if at a typical CEUS site the appropriate ground motion A_R is 2.25 over the natural frequency range of 5 to 10 Hz and A_R is 3.75 at 1.0 Hz, then a median annual probability of failure goal of 1×10^{-6} is reasonably achieved with a HCLPF seismic margin F_H of 1.33 by setting the SSE spectral acceleration at the median 1×10^{-5} annual frequency of exceedance in the 5 to 10 Hz range, and at a median 5×10^{-6} annual frequency of exceedance at 1.0 Hz (see Table 1 for the R_{Pmin} estimates upon which these exceedance frequencies are based). Therefore, in this case, the median annual frequency of exceedance of 1×10^{-5} proposed in Draft Regulatory Guide 1032 works well in the 5 to 10 Hz range if the goal is a 1×10^{-6} annual probability of failure. However, at 1.0 Hz, with an A_R of 3.75 the corresponding K_H is 1.74 which means that the median 1×10^{-5} exceedance frequency spectral acceleration would have to be increased by a factor of $(0.5)^{-1/1.74} = 1.5$ to correspond to a median 1×10^{-6} exceedance frequency.

Many engineers, including this author, do not believe that a UHS set at an annual exceedance frequency appropriate in the 5 to 10 Hz range provides an adequate degree of conservatism at low natural frequencies. The previous paragraph illustrates one of the concerns. A second reason is concern that the spectral accelerations at around 1.0 Hz and lower are more influenced by the displacement time history of the ground motion than by the acceleration time history particularly for earthquakes with magnitudes of 6.0 or less and typical CEUS site conditions. For recorded earthquake ground motions the displacement time history has considerable uncertainty because it is typically obtained by double integrating the recorded acceleration time history record and is thus sensitive to instrument and base line corrections. The low frequency spectral accelerations may be significantly inaccurate and thus underpredicted by at least some attenuation relationships. A third concern is that damage of many ductile components is likely to be most related to the spectral displacement content of the input motion at natural frequencies significantly below the elastic frequency of the component. Therefore, the low frequency spectral accelerations (or spectral displacements) are of considerable importance when predicting the realistic seismic capability of ductile components with elastic frequencies in the 5 to 10 Hz range. Each of these three issues needs to be addressed by any future seismic design criteria in order to reasonably achieve a target minimum annual probability of failure over the entire natural frequency range of interest.

Whereas a deterministic acceptance criteria aimed at achieving a HCLPF seismic margin F_H of about 1.33 coupled with an SSE ground response spectrum defined at the median 1×10^{-5} exceedance frequency in the 5 to 10 Hz range is a reasonable way to achieve a median failure probability of 1×10^{-6} for a typical CEUS site with $A_R = 2.25$, this criteria is excessively conservative for a California site with seismicity dominated by close active faults with high recurrence rates where A_R is likely to be between 1.5 and 1.75 in the 5 to 10 Hz range. For this site, from Table 1 an appropriate R_{Pmin} would be about 30 so that the median exceedance frequency for the SSE should be about 3×10^{-5} in order to achieve a 1×10^{-6} failure probability. However, because of the low A_R ratio, a threefold increase in the hazard exceedance probability does not correspond to a large reduction in the SSE. For example, with $A_R = 1.7$ the corresponding K_H is 4.34 which means that a threefold increase in the exceedance frequency would correspond to a $(3.0)^{-1/4.34} = 0.8$ factor on the SSE spectral accelerations.

In conclusion, establishing the SSE ground motion in terms of a specified annual exceedance frequency as is done in D.R.G.1032 is an essential first step toward establishing a risk based seismic design criteria. However, the use of a constant median exceedance frequency of 1×10^{-5} for all sites and over all natural frequencies is not desirable. Such a criteria may lead to unacceptably high seismic risk for components sensitive to low natural frequencies or other situations where A_R exceeds about 3.0. The required SSE spectral accelerations would be significantly increased when A_R exceeds about 3.0. Conversely, a slight relaxation in the SSE spectral accelerations below the median 1×10^{-5} exceedance frequency values should be permitted when A_R is less than about 2.0. However, this relaxation factor should not be less than about 0.8 for an acceptance criteria geared to a HCLPF seismic margin of 1.33.

2.2 Potential for Improving Seismic Margin Acceptance Criteria

There is no fundamental reason why the seismic margin should be defined in terms of the HCLPF or 1% conditional probability of failure seismic capacity $S_{1\%}$. The seismic margin could just as easily be defined in terms of the 5%, 10%, 15%, or even median (50%) seismic capacity.

When the seismic margin is defined in terms of HCLPF seismic capacity, the probability ratio R_p defined by Eqns. (6) and (7) is sensitive to the logarithmic standard deviation β of the component fragility curve. Table 1 presents the minimum value of R_p over the likely range of β from 0.3 to 0.6. However, as shown in Table 2, the ratio of the maximum to minimum values of R_p over this range of β values ranges from 1.8 to 2.5 over the ground motion ratio A_R range of 1.5 to 6.0. Therefore, the use of the R_{pmin} values from Table 1 can introduce as much as a factor of 2.5 conservatism in establishing the hazard exceedance frequency H_{SSE} corresponding to a target failure probability P_F .

Table 2 also shows the ratio of the maximum to minimum values of R_p obtained when the seismic margin is defined in terms of the 5%, 10%, 15%, 20%, and median conditional probabilities of failure. Note that when the seismic margin is defined in terms of the 20% conditional probability of failure seismic capacity $S_{20\%}$, there is negligible sensitivity of R_p to variation of β over the range of 0.3 to 0.6 for A_R values of 2.5 and greater. However, this lack of sensitivity to β for A_R values of 2.5 and greater is gained at the expense of a much greater sensitivity to β at A_R values of 1.75 and less.

Over A_R values of 2.0 and greater which are appropriate for CEUS sites, the 10% conditional probability of failure seismic capacity $S_{10\%}$ appears to represent a good compromise candidate. Within this range, the ratio of maximum to minimum R_p is sufficiently small to have negligible impact. However, when A_R drops below 1.8, the use of a seismic margin defined in terms of $S_{10\%}$ leads to greater sensitivity of R_p to β than does the use of a HCLPF seismic margin. Overall, defining the required seismic margin in terms of $S_{10\%}$ is preferable to using the HCLPF seismic margin in order to minimize the variation of R_p with β . For this reason, DOE-STD-1020-94 (Ref. 3) aims its deterministic seismic design criteria at achieving a specified required factor of safety $F_{10\%}$ on the 10% conditional failure probability capacity $S_{10\%}$. The relationship between $F_{10\%}$ on the 10% conditional failure probability capacity and F_H on the HCLPF seismic capacity is:

$$(F_{10\%} / F_H) = e^{1.044\beta} \quad (8)$$

Over a β range from 0.3 to 0.6, $(F_{10\%}/F_H)$ ranges from 1.37 to 1.87. Earlier, it was judged that the existing SRP (Ref. 6) typically leads to a HCLPF seismic margin of 1.25 to 1.5 when the SRP criteria is just barely met. On the same basis, one can judge that the SRP criteria typically leads to a $F_{10\%}$ of 1.75 to 2.5.

Table 3 shows the minimum probability ratios R_p corresponding to various safety factors $F_{10\%}$ on the 10% conditional probability of failure seismic capacities. Choice of a $F_{10\%}$ of 1.875, 2.0, or 2.25 should lead to deterministic seismic acceptance criteria which on average is close to existing SRP seismic acceptance criteria. Choice of $F_{10\%}$ of 1.5 or 1.0 would lead to seismic acceptance criteria increasingly more liberal on average than existing SRP criteria.

For $F_{10\%} = 1.0$, from Tables 2 and 3 it can be seen that for A_R from 2.0 to 6.0 and β from 0.3 to 0.6, R_p always lies in the range of 1.5 to 2.4 irrespective of both A_R and β . Therefore, for CEUS sites a potentially attractive seismic design criterion is to define a criterion $R_{pc} = 2.0$ and:

1. Set the SSE exceedance frequency H_{SSE} at two times the target annual seismic-induced failure probability P_F .
2. Define the deterministic acceptance criteria with sufficient conservatism to achieve less than about a 10% conditional probability of failure.

For sites at which A_R is less 2.0, this seismic design criterion becomes unconservative for components with very high β values for the fragility curve. At the extreme ($A_R = 1.5$, and $\beta = 0.6$), the SSE ground motion defined by this criterion would have to be increased by a factor of 1.45 in order to achieve the target failure probability P_F .

Even though the above defined seismic design criterion is theoretically very attractive in that the sensitivity to both A_R and β are minimal for A_R greater than 2.0, it has two practical difficulties. First, to achieve failure probabilities consistent with past design practice, the SSE ground motions would have to be set at a lower exceedance probability (i.e., significantly higher SSE ground motions). Secondly, the deterministic seismic acceptance criteria would have to be significantly more liberal than current SRP and code criteria. Winning general acceptance of using significantly higher SSE ground motions and significantly more liberal acceptance criteria to be used with these ground motions is likely to be difficult.

A second option for a potentially attractive seismic design criterion which leads to deterministic seismic acceptance criteria reasonably similar to current SRP and code criteria is:

1. Set the exceedance frequency H_{SSE} at which the SSE response spectrum is defined at a factor R_{pc} above the target failure probability P_F where:

$$R_{pc} = \frac{20}{A_R} \quad (9)$$

2. Define the deterministic acceptance criteria with sufficient conservatism to achieve about a 1.875 factor of safety on the 10% probability of failure seismic capacity (i.e., $F_{10\%} = 1.875$)

For sites at which A_R is less than 1.6, this seismic design criteria also becomes very slightly unconservative for components with very high fragility β values. At the extreme ($A_R = 1.5$ and $\beta = 0.6$), the SSE ground motion defined by this criterion would have to be increased by a factor of 1.08 in order to achieve the target failure probability. This level of increase is considered to be in the noise level and can be ignored. Over the A_R range from 1.7 to 2.75, this criterion is conservatively biased, and accurately achieves the target failure probability for A_R values of 3.0 and greater.

This second option works well over the entire range of A_R and β values studied. This second option is similar to the approach taken in DOE-STD-1020-94 for DOE Performance Category 4 component which is the DOE category most similar to the NRC nuclear power plant Seismic Category 1.

A third option is:

1. Set the exceedance frequency H_{SSE} at the same level as for the second option, i.e., in accordance with Eqn. (9).
2. Define the deterministic acceptance criteria with sufficient conservatism to achieve about a 1.33 factor of safety on the HCLPF capacity (i.e., $F_H = 1.33$).

This third option is conservatively biased over the entire range of A_R values, particularly for components with large fragility β values. Its primary advantage over the second option is that it does not introduce a new seismic margin definition, but retains the HCLPF seismic margin definition.

The factor F_{SSE} by which the SSE ground motion defined by R_{pc} for any of these options would have to be adjusted to exactly achieve the target failure probability can be obtained from:

$$F_{SSE} = \left(\frac{R_{pc}}{R_p} \right)^{1/K_H} \quad (10)$$

where R_p is defined by Eqn. (6). The maximum and minimum F_{SSE} factors over the range of β from 0.3 to 0.6 are tabulated in Table 4 as a function of A_R for each of the three described options.

From Table 4, it can be seen that Option #2 does the best job of achieving the target failure probability over the entire A_R range. The factor F_{SSE} required on the SSE ground motion to achieve the target failure probability ranges from 0.74 to 1.08 with a mean value of 0.91. On average about a 10% conservative bias in the seismic design criteria is introduced with this option. Option #3 introduces more scatter and generally more conservatism with F_{SSE} ranging from 0.60 to 1.06 with a mean value of 0.82. On average this option introduces about a 20% conservative bias in the seismic design criteria. Based upon these considerations, Option #2 is slightly preferable to Option #3.

However, to satisfy the IPEEE submittal guidance contained in Ref. 1, every operating U.S. nuclear power plant should have estimated the HCLPF seismic margin for seismic safety significant components with low HCLPF capacities. Therefore, at this time a significant level of expertise and a significant data base exists for estimating HCLPF seismic margins which can be used directly in Option #3. It is not clear that Option #2 represents a sufficient improvement over Option #3 to warrant the confusion that might result from introducing an alternate seismic margin definition.

The remainder of this paper will concentrate on Option #3. However, the ideas and concepts introduced are equally applicable if Option #2 were to be ultimately chosen. These ideas are also applicable if a HCLPF seismic margin factor F_H other than 1.33 were chosen. Simply the R_{cp} value would have to be correspondingly modified from that given by Eqn. (9).

3. Derivation of Seismic Criteria to Achieve Desired Seismic Margin

3.1 General Derivation

In this section an overall deterministic seismic criteria approach is derived to achieve a specified seismic margin factor F_H by which the HCLPF seismic capacity exceeds the design SSE ground motion. This derivation specifically applies to components whose failure mode is controlled by either stress, strain, force, displacement, rotation or some other parameter which can be assessed by analysis. However, the general approach can also be applied to functional failure modes which can only be assessed by test.

The general SRP seismic requirements for components qualified by analysis can be expressed by:

$$C_C \geq D_{NS} + D_S \quad (10)$$

where C_C represents a specified code capacity, D_{NS} represents the non-seismic demand to be combined with the seismic demand, and D_S represents the elastic-computed seismic demand calculated in accordance with SRP criteria. A code seismic capacity C_{cs} can be defined as:

$$C_{cs} = C_C - D_{NS} \quad (11)$$

$$C_{cs} \geq D_S \quad (12)$$

The ratio of the median seismic capacity of any component to the design basis SSE can be defined by a median factor of safety $F_{50\%}$ defined by the product of the median capacity factor $F_{C50\%}$, the median response factor of conservatism, $F_{R50\%}$, and the median nonlinear factor $F_{N50\%}$, i.e.:

$$F_{50\%} = (F_{C50\%})(F_{R50\%})(F_{N50\%}) \quad (13)$$

$F_{C50\%}$ represents the factor by which the 50% probability of failure capacity lies above the design capacity C_{cs} . $F_{R50\%}$ represents the amount by which the computed demand D_S lies above the 50% non-exceedance probability (NEP) demand for the SSE input. $F_{N50\%}$ represents the amount that the median elastic computed demand can exceed the median capacity and still have 50% likelihood of acceptable behavior because of such things as the short duration and oscillatory nature of the input motion and the nonlinear hysteretic energy dissipation capability of the component.

In turn, the logarithmic standard deviation (variability) β of the factor of safety can be defined by:

$$\beta = [\beta_C^2 + \beta_R^2 + \beta_N^2]^{1/2} \quad (14)$$

where β_C , β_R , and β_N define the variability of the capacity, response, and nonlinear factor, respectively. The combination of $F_{50\%}$ (median), and β define the composite (mean) fragility curve for the component in terms of a factor times the SSE input. Since the HCLPF seismic capacity corresponds to the 1% conditional probability of failure on this fragility curve, the HCLPF seismic margin factor F_H is given by:

$$F_H = F_{50\%} e^{-2.326\beta} \quad (15)$$

The SRP seismic criteria needs to be set sufficiently conservatively so that F_H approximately equals the desired seismic margin when the criteria is just barely met. One approach to achieve this goal which is believed to result in relatively minimal changes to existing SRP criteria is described.

The code minimum ultimate, code limit-state, or code Service Level D capacities that this author has investigated all lead to a capacity margin $R_{C1\%}$ greater than unity on the 1% NEP capacity. Thus:

$$F_{C50\%} = R_{C1\%} e^{2.326\beta_c} \quad (16)$$

In this author's judgment, response analyses conducted in accordance with the SRP are aimed at the 84% NEP. However, because of either excess conservatism in some aspects of the SRP response requirements, or because of excess conservatism introduced by the analyst, most response analyses achieve a response margin $R_{R84\%}$ greater than unity. Thus:

$$F_{R50\%} = R_{R84\%} e^{\beta_R} \quad (17)$$

Currently, no specific provisions exist in the SRP for incorporating a nonlinear factor F_N . Nonlinear analysis is permitted, but must be reviewed on a case-by-case basis. There is very little precedence where nonlinear analysis has been used for seismic evaluation at U.S. nuclear power plants, and even this limited precedence has primarily been for the evaluation of existing components subjected to revised SSE input above that for which the component was designed. However, for ductile failure modes ignoring F_N introduces excessive conservatism. It is herein proposed that nonlinear analysis should be explicitly permitted with permissible nonlinear distortions aimed at achieving a nonlinear margin $R_{N16\%}$ on the 16% probability of failure distortion level. Thus:

$$F_{N50\%} = R_{N16\%} e^{\beta_N} \quad (18)$$

By combining Equations (13) through (18):

$$F_H = (R_{C1\%}) (R_{R84\%}) (R_{N16\%}) (f_\beta) \quad (19)$$

$$f_\beta = e^{\beta_R + \beta_N - 2.326(\beta - \beta_c)} \quad (20)$$

Typically, for structures and most other primary components, the likely range of β_c is from 0.2 to 0.4 and β_R is from 0.2 to 0.3. The variability β_N is strongly dependent on $F_{N16\%}$ since it is impossible for F_N to drop below unity because unity corresponds to elastic behavior. The author's judgment is that:

$$\beta_N \leq 0.2 [F_{N16\%} - 1.0] \quad (21)$$

For a brittle failure mode, $F_{N16\%} = 1.0$ and $\beta_N = 0$. For a highly ductile failure mode, it is highly unlikely that an appropriate $F_{N16\%}$ would exceed 3.0 which means that β_N is even less likely to exceed 0.4. Table 5 shows that the f_β values computed from Eqn. (20) for the likely ranges of β_c , β_R , and β_N range from 0.93 to 1.21. Therefore, with on-average a slight conservatism:

$$F_H \approx (R_{C1\%}) (R_{R84\%}) (R_{N16\%}) \quad (22)$$

Thus, the HCLPF seismic capacity ($F_H = 1.0$) can be computed by:

1. Setting the seismic capacity at the 1% NEP capacity level.
2. Computing the seismic demand at the 84% NEP level.
3. If the failure mode is ductile, perform a nonlinear evaluation and permit nonlinear distortions up to the 16% failure probability level.

In order to achieve a HCLPF seismic margin of 1.33, the HCLPF capacity obtained should exceed the SSE ground motion by a factor of 1.33. This 1.33 factor can be composed of any product combination of $R_{C1\%}$, $R_{R84\%}$, $R_{N16\%}$. Specific suggestions for these factors will be provided in the next section.

3.2 Situation Which Might Warrant Increased Margin

Because of their wide dispersion throughout the plant, and the large variability in details, piping systems contain a large number of potential failure locations. Failure probabilities at such locations are neither purely dependent or purely independent. Furthermore, only a few of these locations are likely to be critical for any given piping system. Even so, because of the large number of at-least partially independent piping segments, piping will control the plant HCLPF capacity unless piping has substantially less than a 1% probability of failure at the Plant HCLPF capacity. In order to provide reasonable assurance that piping will not control the plant HCLPF capacity, the probability of failure P_{PF} of a piping segment should be limited to significantly less than 1% at the plant HCLPF seismic capacity level. It is impossible to rigorously define at how much less than 1% this failure probability should be set without performing detailed plant evaluation studies. However, it is judged that seismic design criteria for piping should be aimed at achieving a conditional piping failure probability P_{PF} in the range of 0.02% to 0.5% at the plant HCLPF level. For the purpose of illustrating the influence of assigning this lower failure probability level P_{PF} on the seismic design criteria, it will be assumed that:

$$P_{PF} \leq 0.1\% \text{ at Plant HCLPF Level} \quad (23)$$

To achieve this lesser failure probability, the factor $f_{\beta P}$ defined in Eqn. (20) must be modified to:

$$\begin{aligned} f_{\beta P} &= e^{\beta_{RP} + \beta_N + X_{CP}\beta_{CP} - X_{TP}\beta_P} \\ X_{CP} &= 2.326 \quad (1\% \text{ NEP}) \\ X_{TP} &= 3.090 \quad (0.1\% \text{ NEP}) \end{aligned} \quad (24)$$

and the required HCLPF seismic margin factor F_{HP} for piping becomes:

$$F_{HP} = F_H (f_{\beta P})^{-1} \quad (25)$$

where F_H is the desired seismic margin for the plant. Thus, $(f_{\beta P})^{-1}$ becomes the required increased factor of conservatism for piping seismic design criteria.

For distributed piping systems, the capacity variability β_{CP} and possibly the response variability β_{RP} are typically larger than those tabulated in Table 5 for structures and other non-distribution system components. In addition, the coefficient X_{TP} of 3.090 in Eqn. (24) for $f_{\beta P}$ associated with 0.1% NEP is

larger than the corresponding coefficient of 2.326 in Equation 20 for f_p associated with 1% NEP. Table 6 presents the resulting estimates for $(f_{BP})^{-1}$ for piping systems.

This same approach was also used to obtain $(f_{BP})^{-1}$ factors corresponding to failure probabilities P_{PF} of 0.02% and 0.5%. With, on average, a slight level of conservatism, it is recommended that:

P_{PF}	$(f_{BP})^{-1}$	F_{HP}
0.5%	1.1	1.5
0.1%	1.5	2.0
0.02%	2.0	2.67

For situations where the issue of the combination of multiple independent failure modes becomes important, it is probably desirable to increase the component HCLPF seismic margin to the range of 1.5 to 2.67 in order to achieve a plant HCLPF seismic margin of 1.33. This increase will not be included in the discussions contained in the remaining sections, but it could be easily included where deemed appropriate.

4. Recommendations for Margin Based Seismic Design Criteria

4.1 Seismic Response (Demand) Criteria

The response analysis criteria of the SRP should be aimed at computing the seismic demand D_s at the 84% NEP level. This level of conservatism should be the goal irrespective of whether the demand quantity of interest is a shear or moment in a major structure, a floor spectra input to a component, a local stress in a component, or the in-cabinet spectrum applied to a relay in a component mounted at either a high or low elevation within a structure.

In summary, the response goal is for:

$$R_{R84\%} \approx 1.0 \quad (25)$$

It is recognized that seismic analysts will often introduce additional response conservatism for various reasons. One of the most likely reasons is to enable the response analysis to be simplified. However, this additional level of conservatism is unreliable and should not be taken credit for when developing SRP or code criteria for capacity to satisfy Eqn. (22).

In general, the 1989 Revision 2 of the SRP reasonably achieves the goal of seismic demands being computed at about the 84% NEP level by the following approach:

1. The SSE is defined in terms of an earthquake magnitude and distance. For this magnitude and distance, the SSE response spectrum is defined at the 84% NEP level.
2. For soil sites, soil stiffnesses are varied by approximately one standard deviation to obtain reasonable upper and lower bound properties as well as best estimate properties. Seismic demands computed over this range of properties are enveloped and the largest demands computed within this property range are used.
3. Computed in-structure response spectra are frequency shifted at least $\pm 15\%$ to account for a reasonable level of uncertainty in structure frequencies.

4. Slightly conservative estimates of median damping levels are specified.
5. In all other aspects the demand analysis is essentially median centered.

In this author's opinion, the SRP could more closely achieve 84% NEP computed seismic demands if it were brought into closer agreement with ASCE Standard 4-86 (Ref. 8). The SRP response combination requirements are excessively conservative in two areas. First, closely spaced modes should be combined algebraically rather than absolutely. Secondly, for distribution systems such as piping with multiple supports with differing input motion, the current practice is to either envelope the response spectrum for each support and apply this envelop spectrum to all supports, or to independently compute response for each support input and to combine these independently computed demands absolutely. However, the sometimes proposed suggestion to combine responses from these individual support motions by the square-root-sum-of-squares (SRSS) approach is equally invalid and can lead to significant unconservatism in some cases. Specific and known relative phasing exists at each natural frequency between the motions at different supports because each support motion is made up of a combination of multiple modal building responses for which independent locations are either perfectly in phase or perfectly out-of-phase, plus the input ground motion which produces the same phasing at each support. Both the closely spaced mode and independent support motion criteria can be easily improved so as to enable the SRP criteria to more closely achieve 84% NEP computed seismic demands when the seismic input is defined at the 84% NEP level.

However, when the SSE response spectrum is defined in terms of either a mean or median annual frequency of exceedance, the seismic input no longer corresponds to the 84% NEP level. Therefore responses computed in accordance with the guidance of either the SRP (Ref. 6), or ASCE Std. 4-86 (Ref. 8) will not lie at the 84% NEP level. The problem is that the response spectrum from any real ground motion will have local peaks and valleys. The natural frequencies at which these local peaks and valleys occur are random and cannot be predicted for some future ground motion. Therefore, even if on average this real response spectrum matches the annual exceedance frequency defined SSE response spectrum, at 50% of the natural frequencies the real response spectrum will exceed the SSE response spectrum.

The rigorous ways to compute 84% NEP responses corresponding to an annual exceedance frequency defined SSE response spectrum is to specify 15 to 30 real or artificial time histories which reasonably replicate the response spectra peak and valley variability obtained in response spectra from actual ground motion records. Each time history should then be scaled so that the ratio of the spectral acceleration from the time history to the SSE spectral acceleration averages unity over some broad natural frequency range of primary interest. Thus, on average, each time history serves as a reasonable SSE input motion. In addition, the average spectral acceleration from all 15 to 30 records should closely match the SSE response spectrum at every natural frequency. Next, 15 to 30 structural response models are developed with each model having a different combination of soil stiffness, structural stiffnesses, and damping selected to reasonably approximate the variability of each of these parameters. These models are randomly combined with the time history records and 15 to 30 analyses are performed. The design seismic responses should be the responses that are exceeded in only 16% of these analyses. For instance, if 30 response analyses are performed, the design seismic response should correspond to the 5th highest result.

The above described approach of performing multiple analyses should be defined and permitted by the SRP so as to most rigorously obtain an 84% NEP response. However, it requires considerable work and should not be required. In the author's experience, a reasonable and slightly conservative estimate of the 84% NEP response can be obtained using a single analysis conducted in accordance with existing SRP

requirement for frequency shifting and SSI enveloping so long as the results are scaled by a factor of about 1.2 to 1.25 to account for the peak and valley variability of the input motion.

In summary, it is appropriate to define the SSE response spectrum in terms of either a mean or median annual frequency of exceedance as is proposed in DRG 1032 (Ref. 2). In fact, the SSE response spectrum must be so defined if the seismic design criteria is to be aimed at achieving some defined seismic risk goal defined in terms of an annual probability of failure. However, when defined in this way, either multiple analyses with multiple time-history inputs need to be performed as described above, or the response results of a single analysis need to be increased by a scale factor SF so as to approximately correspond to 84% NEP demands. It is tentatively suggested that:

$$SF \approx 1.2 \text{ to } 1.25 \quad (27)$$

4.2 Seismic Capacity Criteria

4.2.1 Capacity Criteria for Brittle Failure Modes

A number of failure modes occur with very little benefit from nonlinear behavior. Examples are the failure of a weld anchoring a cabinet, pullout of anchorage in the concrete, connection failures in steel frames, out-of-plane shear failure of concrete beams without shear reinforcement, and compressive elastic buckling of steel braces. With only slight conservatism, these failure modes can be considered to be brittle with no credit being taken for nonlinear behavior. Therefore, the entire desired HCLPF seismic margin factor F_H must be applied to the code capacity if the seismic demand is defined at the 84% NEP level.

In summary a 1.33 HCLPF seismic margin is approximately achieved for brittle failure modes by:

1. Requiring the code seismic capacity C_s to exceed the 84% NEP computed seismic demand, and
2. Setting the code seismic capacity sufficiently conservative to achieve a 1.33 factor of safety on the 1% NEP capacity.

Conservatism in code capacities is generally achieved by both conservatively specifying code material strengths, and through the use of a conservatively biased code limit state equation. Conservatism in the code limit state equation is typically obtained by applying either a strength reduction factor (ϕ factor) or a factor of safety to a nominal limit state equation which might also be conservatively biased.

In general, code specified material strengths typically lie at about the 5% NEP. Therefore, if the code limit state equation is specified with sufficient conservatism to achieve a factor of safety $R_{L5\%}$ on the 5% NEP limit state equation, then the resulting code capacity factor $R_{C1\%}$ can be estimated from:

$$R_{C1\%} = R_{L5\%} e^{f_c} \quad (28)$$

$$f_c = 1.645(\beta_M + \beta_L) - 2.326\beta_c \quad (29)$$

$$\beta_c = [\beta_M^2 + \beta_L^2]^{1/2} \quad (30)$$

where β_M is the variability in capacity due to material property variability and β_L is the limit state capacity variability when material properties are known. For practical problems:

$$0.5 \leq \left(\frac{\beta_M}{\beta_L} \right) \leq 2.0 \quad (31)$$

and from Eqn. (29) for $\beta_c \leq 0.4$:

$$0.95 \leq e^{fc} \leq 1.00 \quad (32)$$

so that:

$$R_{C1\%} \approx R_{L5\%} \quad (33)$$

Thus, to achieve a 1.33 HCLPF seismic margin, the code limit state equation should be established sufficiently conservatively to achieve about a 1.33 factor of safety on the 5% NEP limit state equation for brittle failure modes.

4.2.2 Seismic Capacity Criteria for Ductile Failure Modes

Many failure modes exhibit significant ductility prior to failure. Examples are in-plane shear failure of shear walls and braced frames, flexural failures in general, and any other situation where significant yielding occurs prior to failure. Code capacities generally take partial credit for this nonlinear behavior by establishing the code limit state equation less conservatively for ductile failure modes than for brittle failure modes. This practice is considered appropriate.

In order to achieve a 1.33 HCLPF seismic margin (ie., $F_H = 1.33$) it is recommended that:

$$\begin{aligned} R_{C1\%} &= 1.0 \\ R_{N16\%} &= 1.33 \end{aligned} \quad (34)$$

since F_H is equal to the product of these two factors. In other words, the desired seismic margin is achieved by limiting the amount of credit which may be taken for nonlinear behavior.

For ductile failure modes, it is suggested that either elastic or nonlinear analysis be permitted. The following criteria is proposed to achieve a 1.33 HCLPF seismic margin.

Elastic Evaluation

Set seismic capacity at the 1% NEP capacity and require this code capacity to exceed the 84% NEP elastic computed seismic demand. However, if this provision is not met, a nonlinear evaluation should be permitted.

Nonlinear Evaluation

For a nonlinear evaluation, the 84% NEP seismic demand should be increased by a nonlinear margin factor SF_N of 1.33 to achieve the desired HCLPF margin. Note that this nonlinear margin factor is in addition to the scale factor SF defined by Eqn. (24) needed to achieve an 84% NEP demand for an exceedance frequency based SSE response spectrum. Thus the total scale factor SF_T becomes:

$$SF_T = (SF_N)(SF) \approx 1.6 \text{ to } 1.67 \quad (35)$$

unless 15 to 30 nonlinear analyses are performed and the nonlinear response is computed at the 84% NEP level in which case a nonlinear scale factor SF_N is still required.

The capacity of the nonlinear hysteretic model should be limited to the same seismic code capacity level as that described above for elastic evaluation. For acceptance, the computed nonlinear distortions (strains, displacements, or element rotational hinges) should be less than the estimated 16% probability of failure distortion level.

4.2.3 Seismic Capacity Criteria for Components Qualified By Test

Current SRP and code provisions require that a broad frequency Test Response Spectrum (TRS) envelope the Required Response Spectrum (RRS) by a factor of at least 1.1 where the RRS is defined by a broadened in-structure response spectrum. However, when the RRS is defined at the 84% NEP level, this 1.1 factor is insufficient to achieve a 1.33 HCLPF seismic margin for a broad frequency RRS.

For a broad frequency RRS, the required factor F_{TB} between the TRS and the RRS should be:

$$F_{TB} = 1.33e^{f_T \beta_c} \quad (36)$$

$$f_T = (2.326 - X_{PT})$$

to provide a factor of safety of 1.33 on the 1% probability of failure capacity, where β_c represents the sample to sample variability (logarithmic standard deviation) for the tested component, and X_{PT} represents the standard normal variant corresponding to the confidence provided by the test of acceptable equipment performance at the TRS level.

Based upon a review of fragility results presented in Bandyopadhyay, et al. (Ref. 9), β_c is estimated to be about 0.20 for equipment qualified by test. The value of X_{PT} to use must be based on judgment and should preferably be established by the consensus of a committee of experts. However, based upon Appendices J and Q of Ref. 10, for the purpose of this example, it is judged that such qualification testing provides somewhere between 90% and 98% confidence of acceptable equipment performance at the TRS level. Thus:

Assumed Confidence Level	X_{PT}	F_{TP}
98%	2.054	1.4
95%	1.645	1.5
90%	1.282	1.65

However, most in-structure response spectra have a narrow bandwidth (ie., narrow frequency content) particularly for spectra at high elevations in structures. Experimental observations by Merz (Ref. 11) and others indicate that a narrow frequency input spectrum must be scaled to a higher level than a broad frequency input spectrum in order to produce relay chatter or structural damage. Merz states:

"The peak values of narrow-band amplified mounting response cannot be compared directly with the broad-band type of inputs used in multi-axis relay testing which form the basis of relay GERS..."

For complex devices such as relays, a narrow-band input is judged to be less severe from a fragility standpoint than a broad-band input, due to the absence of multi-mode response (Ref. 12), variable RMS severity over the bandwidth of test spectra (Ref. 13), and the lack of interaction of nonlinear responses in the narrow-band case. Recent studies (Ref. 13 through 16) have concluded that a constant correction factor can be applied to narrow-band data to produce an approximately equivalent broad-band result."

Both Merz (Ref. 11) and Kana (Refs. 13 and 17) suggest that a correction C_B should be applied to the narrow frequency high spectral peaks of the RRS to produce a damage-effective RRS.

Kana (Refs. 11 and 12) has suggested that equipment malfunction is dependent not only on peak spectral amplitude, but also on the RMS amplitude of the wave form, and has developed relationships between the mean ratio of the RMS to peak spectral amplitude as a function of the bandwidth to central frequency ratio B as defined in (Ref. 13 and 14):

$$B = \frac{\Delta f_{0.8}}{f_c} \quad (37)$$

where $\Delta f_{0.8}$ is the total frequency range over which spectral amplitudes exceed 80% of the peak spectral amplitude and f_c is a central frequency for the frequencies which exceed 80% of the peak amplitude. Assuming damage is purely dependent on the RMS amplitude of the waveform and is independent of the peak spectral amplitude, Kana (Ref. 11 and 12) has suggested a relationship between a correction factor C'_B and B which can be closely approximated by:

$$C'_B \approx 0.32 + 0.4B \quad (0.15 \leq B \leq 0.9) \quad (38)$$

Because Eqn. (38) is based on the assumption that damage is purely dependent on RMS amplitude, C'_B provides a lower bound (unconservative) estimate of an approximate reduction factor C_B . If damages were purely dependent on peak spectral amplitude, then C_B would be unity (no correction). In reality, C_B lies somewhere between C'_B and unity, but probably much closer to C'_B than to unity.

The minimum required ratio F_T between a broad frequency TRS and the peak spectral acceleration of an 84% NEP RRS should be:

$$F_T = \left(\frac{\text{TRS}}{\text{RRS}} \right) = F_{TB} C_B \quad (39)$$

In-structure spectra (5% damping) at high elevations within civil structures will tend to have B ratios in the range of 0.15 to 0.6. So long as the bandwidth factor B is less than 0.6, it seems reasonable to retain the current requirement that the ratio (TRS/RRS) be at least 1.1. However, for a broad frequency ground

response spectrum, B is typically about 1.5 and no C_B correction should be applied. In this case it would be more reasonable to require a (TRS/RRS) ratio of about 1.5.

In summary, it is suggested that to achieve a 1.33 HCLPF seismic margin, the required ratio of (TRS/RRS) should be a function of the bandwidth factor B of the RRS. For this purpose, the bandwidth factor B should be defined in terms of the unbroadened in-structure response spectrum. A reasonable requirement would appear to be:

B	(TRS/RRS)
≤ 0.6	1.1
≥ 1.5	1.5

with linear interpolation for B values between 0.6 and 1.5.

4.3 Comparisons To Other Seismic Margin Based Criteria

The seismic design criteria presented herein is heavily based upon and similar to the Conservative Deterministic Failure Margin (CDFM) criteria for computing HCLPF seismic margins originally proposed in Ref. 19, and extensively expanded upon in Ref. 10, with examples provided in Ref. 20. The ideas for computing a probability ratio between the desired seismic failure probability and the seismic exceedance frequency for the SSE response spectrum is based on the work performed to develop the DOE-STD-1020 (Ref. 3) as expanded upon in Ref. 4. The resulting criteria presented herein is similar to the DOE seismic design criteria for Performance Category 4 components.

The primary difference between the proposed seismic design criteria and the CDFM criteria is that the CDFM criteria is aimed at computing a HCLPF seismic capacity while the proposed design criteria is aimed at achieving a 1.33 seismic margin by which the HCLPF capacity exceeds the design SSE response spectrum. Both define the demand (response) in terms of the 84 NEP demand. For brittle failure modes, both are based upon defining the capacity in terms of the 1% NEP capacity. However, for ductile failure modes a slight difference exists. The CDFM ductile failure mode capacity is defined in terms of the 2% NEP capacity and a nonlinear factor $F_N = 1.25$ is used in lieu of the nonlinear analysis suggested herein. The criteria proposed herein eliminates some of the excess conservatism that exists in the CDFM criteria for ductile failure modes.

The primary difference between the proposed criteria and the DOE seismic design criteria for Performance Category 4 components is that the DOE criteria aims at achieving a factor of safety of 1.875 against the 10% NEP conditional probability of failure capacity. This goal is slightly less conservative than the goal of a 1.33 HCLPF seismic margin. The DOE criteria is more median centered on achieving the desired annual failure probability while the criteria presented herein introduces a conservative bias through the use of the HCLPF capacity as was shown by the comparison of Options #2 and #3 in Table 4.

The DOE criteria requires that the seismic demand computed from an exceedance probability defined design response spectrum be increased by a scale factor SF of 1.25 which is similar to the 1.2 to 1.25 range proposed herein. The DOE nonlinear factor F_N is aimed at the 5% probability of failure distortions which is slightly more liberal than requiring a safety factor of 1.33 on the 16% probability of failure distortions as proposed herein. Either criteria serves as a reasonable seismic margin based design criteria which can be used to aim at a specified seismic risk goal.

4.4 Example Application of Margin Based Seismic Design Capacity Goals

Questions are always arising as to whether a specific code capacity provides an adequate seismic margin. These questions can be more easily addressed if a target seismic capacity goal is established as has been done in Sections 4.2.1 and 4.2.2 for brittle and ductile failure modes, respectively. Furthermore, over time, greater consistency can be achieved between various code capacity provisions if target seismic capacity goals are established. Thus, when questions come up concerning a proposed revision in a code capacity such as the recent revision in piping stress allowable for seismic loading, it should be easier to reach a consensus if the desired seismic capacity margin goal has been previously agreed upon.

As an example application, it is assumed that the target capacity goal for longitudinal shear failure of fillet welded connections is a factor of safety of 1.33 on the 1% NEP capacity as proposed herein for brittle failure modes. It is desired to determine whether the LRFD provisions of the AISC Code (Ref. 21) roughly achieve this goal.

Based upon extensive testing of fillet welds under longitudinal shear reported in Refs. 22 and 23, the median shear strength t_{50} of the fillet weld can be defined in terms of the median ultimate strength σ_{50} of the electrode by:

$$t_{50} = 0.84\sigma_{50} \quad (40)$$

with an equation logarithmic standard deviation, β_{EQN} , of 0.11. The median ultimate strength is defined in terms of the minimum code nominal tensile strength, F_{EXX} , by:

$$\sigma_{50} = 1.1F_{EXX} \quad (41)$$

with a logarithmic standard deviation, β_{MAT} , of 0.05. In addition, a logarithmic standard deviation, β_{FAB} , of 0.15 due to fabrication tolerances should be considered for normal welding practice. The code shear capacity t_c specified in AISC-LRFD (Ref. 21) for the limit state strength approach for design is:

$$t_c = 0.75(0.6)F_{EXX} \quad (42)$$

Thus the median capacity factor of safety F_C is :

$$F_{C50} = \frac{1.1(0.84)}{0.75(0.6)} = 2.05$$

with the capacity logarithmic standard deviation, β_c , estimated to be:

$$\beta_C = (\beta_{EQN}^2 + \beta_{MAT}^2 + \beta_{FAB}^2)^{1/2} = [(0.11)^2 + (0.05)^2 + (0.15)^2]^{1/2} = 0.19$$

Thus, the capacity margin $R_{C1\%}$ from Eqn. (16) is:

$$R_{C1\%} = 2.05e^{-2.326(0.19)} = 1.32$$

which is very close to the desired factor of 1.33.

The author has performed similar checks on a number of the AISC-LRFD, ACI Ultimate Strength, and ASME Service Level D Capacity provisions and believes that most of these provisions reasonably meet the capacity goals defined in Sections 4.2.1 and 4.2.2. Thus, if the seismic margin based capacity goals of these sections are selected, no wholesale changes in code capacities should be required.

5. Suggested Revisions to SRP Seismic Provisions

In order to better achieve a seismic annual probability of failure risk goal, four significant revisions to the SRP seismic provisions of Refs. 2 and 6 are suggested.

First, as discussed in Section 2, a constant SSE exceedance frequency H_{SSE} should not be established for all sites or over the entire natural frequency range at any particular site. Instead, a target annual probability of seismic-induced failure should be established for Seismic Category 1 components. The permissible SSE exceedance frequency should be a function of the slope of the hazard curve which can be defined by the ratio A_R of the ground motion over a 10-fold change in the annual frequency of exceedance. If the seismic design criteria are aimed at producing either a 1.33 HCLPF seismic margin, or a 1.875 margin on the 10% conditional failure probability capacity, then the SSE exceedance frequency H_{SSE} can be established at a factor R_{pc} given by either Table 1 or Eqn. (9) above the target annual failure probability. If a median 1×10^{-5} exceedance frequency H_{SSE} is appropriate for a typical CEUS site over the 5 to 10 Hz frequency range, the spectral acceleration at 1 Hz should typically be a factor of 1.5 times the median 1×10^{-5} exceedance frequency spectral acceleration because A_R is typically much greater at 1 Hz than it is in the 5 to 10 Hz range.

Secondly, as discussed in Section 4.1, if the seismic response analysis is performed for a single input motion that matches the exceedance frequency defined SSE response spectrum, the computed seismic demands should be scaled by a scale factor of about 1.2 to 1.25 in order to approximate 84% NEP demands. Alternately, multiple analyses using multiple seismic inputs should be performed and the SSE demands should be established at the 84% NEP results from these multiple analyses as described in Section 4.1.

Third, the SRP should explicitly allow nonlinear analyses to determine the seismic capability of components whose failure is ductile. Unless nonlinear analysis is allowed and encouraged, a HCLPF seismic margin capability cannot be realistically determined for such components. As a result, excessive conservatism is introduced for ductile failure modes. Suggested guidance for nonlinear evaluation criteria aimed at a 1.33 HCLPF seismic margin for ductile failure modes is given in Section 4.2.2.

Lastly, the current requirement for components qualified by test that the Test Response Spectrum (TRS) only needs to exceed the Required Response Spectrum (RRS) by a factor of 1.1 does not provide adequate conservatism to achieve a 1.33 HCLPF seismic margin when the RRS has broad frequency content and is defined at the 84% NEP level. In Section 4.2.3, it is suggested that the ratio (TRS/RRS) should be a function of the bandwidth of the unbroadened in-structure RRS. For a broad frequency RRS a (TRS/RRS) ratio of 1.5 is suggested. Since most in-structure response spectra have narrow frequency content, this increase should be necessary in only a limited number of situations.

6. Concluding Remarks

A framework for developing seismic design criteria aimed at any desired seismic risk goal defined in terms of an annual probability of seismic-induced failure is presented. An integral part of this framework is the establishment of an acceptable seismic margin above the design SSE response spectrum input. Three options are suggested in Section 2.2 for this acceptable seismic margin and the advantages and disadvantages of each are discussed. Next, specific goals must be established for both the seismic demand analysis and the seismic capacity evaluation in order to approximately achieve whatever target seismic margin has been established. A candidate set of seismic demand and capacity goals are proposed in Section 4 to achieve a 1.33 HCLPF seismic margin. These target criteria are based on the derivation contained in Section 3. Lastly, a specific set of suggested changes to the SRP seismic provisions are provided in Section 5 to better achieve a seismic risk and seismic margin based design criteria.

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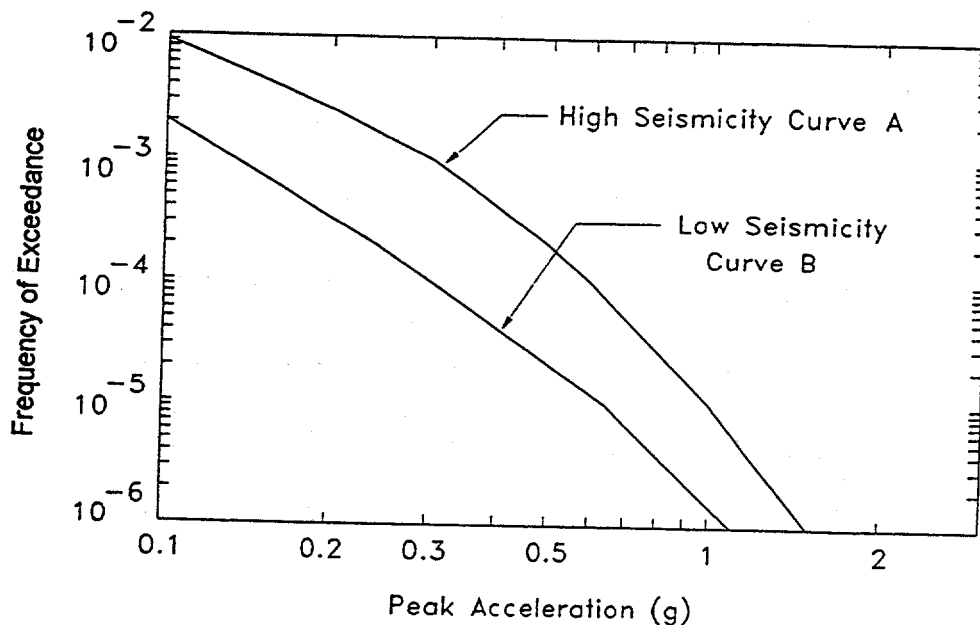


Figure 1 Typical Probabilistic Seismic Hazard Curves

**Table 1: Minimum Probability Ratio R_{pmin} As a Function
of HCLPF Seismic Margin and Ground Motion Ratio
(evaluated over β range from 0.3 to 0.6)**

A_R	R_{pmin}				
	$F_H = 1.0$	1.25	1.33	1.5	1.67
1.5	8.3	30.	42.	83.	153.
1.75	8.2	21.	27.	44.	68
2.0	6.2	13.	16.	24.	34.
2.5	4.4	7.6	8.9	12.0	16.
3.0	3.5	5.6	6.4	8.3	10.4
3.5	3.1	4.7	5.2	6.5	8.0
4.0	2.8	4.1	4.5	5.5	6.6
5.0	2.5	3.4	3.7	4.4	5.2
6.0	2.3	3.0	3.3	3.8	4.4

**Table 2: Ratio of R_{pmax}/R_{pmin} over $\beta = 0.3$ to 0.6 Range
for Various Seismic Margin Definitions**

A_R	Ratio R_{pmax} / R_{pmin}					
	HCLPF $S_{1\%}$ $X_p = 2.326$	$S_{5\%}$ $X_p = 1.695$	$S_{10\%}$ $X_p = 1.282$	$S_{15\%}$ $X_p = 1.037$	$S_{20\%}$ $x_p = 0.842$	$S_{50\%}$ $X_p = 0$
1.5	1.8	4.7	8.8	13.	19.	78.
1.75	1.8	1.4	2.0	2.7	3.5	9.8
2.0	2.3	1.2	1.3	1.6	1.9	4.4
2.5	2.5	1.5	1.2	1.1	1.2	2.3
3.0	2.4	1.6	1.2	1.1	1.1	1.8
3.5	2.3	1.6	1.3	1.1	1.0	1.6
4.0	2.2	1.6	1.3	1.2	1.1	1.5
5.0	2.1	1.5	1.3	1.2	1.1	1.3
6.0	2.0	1.5	1.3	1.2	1.1	1.2

**Table 3: Minimum Probability Ratio R_{pmin} As a Function of $S_{10\%}$ Seismic Margin and Ground Motion Ratio
(evaluated over β range from 0.3 to 0.6)**

A_R	R_{pmin}				
	$F_{10\%} = 1.0$	1.50	1.875	2.0	2.25
1.5	0.24	2.4	8.4	12.2	24.
1.75	1.1	6.0	15.	19.	32.
2.0	1.8	6.8	14.	18.	26.
2.5	2.0	5.5	9.6	11.3	15.
3.0	1.8	4.3	6.9	7.9	10.0
3.5	1.7	3.7	5.5	6.2	7.7
4.0	1.7	3.3	4.8	5.3	6.4
5.0	1.6	2.8	3.9	4.3	5.0
6.0	1.5	2.6	3.4	3.7	4.3

**Table 4: Adjustment Factor F_{SSE} on the SSE Ground Motion
Required to Achieve Target Failure Probability
(β range of 0.3 to 0.6)**

A_R	F_{SSE}					
	Option #1		Option #2		Option #3	
	Max	Min	Max	Min	Max	Min
1.5	1.45	0.99	1.08	0.74	0.82	0.74
1.75	1.14	0.96	0.94	0.79	0.81	0.70
2.0	1.03	0.96	0.90	0.83	0.87	0.68
2.5	1.00	0.95	0.93	0.88	0.96	0.67
3.0	1.04	0.94	0.99	0.89	1.02	0.67
3.5	1.07	0.94	1.02	0.89	1.05	0.67
4.0	1.11	0.94	1.03	0.88	1.06	0.66
5.0	1.18	0.97	1.02	0.84	1.05	0.64
6.0	1.21	1.00	0.98	0.80	1.01	0.60

**Table 5: Typical Values for Factor f_β for Structures
and Most Other components**

β_C	β_R	f_β		
		$\beta_N = 0.0$	0.2	0.4
0.2	0.2	1.01	1.06	0.93
	0.3	0.93	1.01	0.92
0.3	0.2	1.06	1.15	1.05
	0.3	1.01	1.11	1.04
0.4	0.2	1.09	1.21	1.14
	0.3	1.07	1.19	1.15

**Table 6: Typical Values for Factor $(f_{\beta p})^{-1}$ for Piping and
Other Extensive Distribution Systems**

β_{cp}	β_{RP}	$(f_{\beta p})^{-1}$		
		$\beta_N = 0$	0.2	0.4
0.3	0.2	1.24	1.19	1.44
	0.3	1.37	1.29	1.50
	0.4	1.56	1.44	1.62
0.45	0.2	1.32	1.22	1.37
	0.3	1.38	1.27	1.39
	0.4	1.51	1.37	1.47
0.6	0.2	1.43	1.29	1.37
	0.3	1.46	1.31	1.37
	0.4	1.54	1.37	1.42

SOME CONSIDERATIONS FOR ESTABLISHING SEISMIC DESIGN CRITERIA FOR NUCLEAR PLANT PIPING

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SUMMARY

The Energy Technology Engineering Center (ETEC) is providing assistance to the U.S. NRC in developing regulatory positions on the seismic analysis of piping. As part of this effort, ETEC previously performed reviews of the ASME Code, Section III piping seismic design criteria as revised by the 1994 Addenda. These revised criteria were based on evaluations by the ASME Special Task Group on Integrated Piping Criteria (STGIPC) and the Technical Core Group (TCG) of the Advanced Reactor Corporation (ARC) of the earlier joint Electric Power Research Institute (EPRI)/NRC Piping & Fitting Dynamic Reliability (PFDR) program.

Previous ETEC evaluations reported at the 23rd WRSN of seismic margins associated with the revised criteria are reviewed. These evaluations had concluded, in part, that although margins for the tuned PFDR tests appeared acceptable (>2), margins in detuned tests could be unacceptable (<1). This conclusion was based primarily on margin reduction factors (MRFs) developed by the ASME STGIPC and ARC/TCG from realistic analyses of PFDR test 36.

This paper reports more recent results including: 1) an approach developed for establishing appropriate seismic margins based on PRA considerations, 2) independent assessments of frequency effects on margins, 3) the development of margins based on failure mode considerations, and 4) the implications of Code Section III rules for Section XI.

SOME CONSIDERATIONS FOR ESTABLISHING SEISMIC DESIGN CRITERIA FOR NUCLEAR POWER PLANT PIPING¹

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ABSTRACT

The Energy Technology Engineering Center (ETEC) is providing the US NRC technical assistance in: 1) reviewing and evaluating the technical bases for the revised seismic design criteria for nuclear plant piping in the 1994 Addenda to the ASME Code, and 2) evaluating the cumulative impact of the revised criteria on the overall safety margins of piping systems. Issues considered in the evaluation of the basis of the revised criteria are identified and discussed. Issues considered include the appropriate definition and levels of seismic margins, quantification procedures for margins, factors affecting margins, and the development of appropriate design criteria.

The results of previous ETEC evaluations are reviewed, current evaluations presented, and the implications of these evaluations for appropriate design criteria are discussed.

1. INTRODUCTION

The Energy Technology Engineering Center (ETEC) is providing the US NRC technical assistance in 1) reviewing and evaluating the technical bases for the revised seismic design criteria for nuclear plant piping in the 1994 Addenda to the ASME Code, and 2) evaluating the cumulative impact of the revised criteria on the overall safety margins of piping systems. During the reviews and evaluations of these technical bases, a number of issues were identified which are considered herein within the framework of criteria development based on a design by analysis philosophy. As described in Reference 1, in this design approach, criteria are based on fundamental considerations of analysis and material behavior, and there is a unifying philosophy of design in the sense that an attempt has been made to understand all possible modes of failure and provide rational margins of safety against each type of failure in a manner consistent with the consequences of that type.

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² The views expressed in this paper are those of the authors and should not be construed to reflect the US NRC position.

2. BACKGROUND

The ASME Boiler and Pressure Vessel Code (Code) Section III seismic design criteria for nuclear plant piping were revised significantly by the Code 1994 Addenda. The revised criteria are the culmination of over ten years of industry and NRC efforts which were initiated in response to reviews initiated in 1983. These efforts included, in part, the joint Electric Power Research Institute (EPRI)/NRC Piping & Fitting Dynamic Reliability (PFDR) Program and evaluations by the ASME Code Special Task Group on Integrated Piping Criteria (STGIPC) and the Technical Core Group (TCG) of the Advanced Reactor Corporation (ARC) in support of the revised criteria. The results of previous review of these efforts were described in Reference 2 and reported at the 23rd WRSN in 1995.

For convenience, an overview of these efforts are provided in the following. Details are provided in Reference 2.

2.1 Piping & Fitting Dynamic Reliability (PFDR) Program

The objective of the PFDR program (Reference 3) were:

1. To identify failure mechanisms and failure levels of piping components and systems under dynamic loading.
2. To provide a database that will improve the prediction of piping system response and failure due to high level dynamic loads.
3. To develop an improved, realistic and defensible set of piping design rules for inclusion into the ASME Code.

The program included three types of tests: pipe component tests, pipe system tests, and specimen fatigue-ratchet tests, of which only the pipe component tests are reviewed below. Results of the program are provided in References 4 through 8.

The PFDR Component tests included elbows, tees, reducers, nozzles, and supports which were subjected to simulated dynamic seismic, hydrodynamic and water hammer (low-, mid- and high-frequency, respectively) loadings. Components ranged between four- and eight- inches in diameter, were between Schedule 10 and 80 in thickness, and were fabricated from carbon or stainless steel. A total of 41 pipe components were tested. Of these 41 components, 33 were subjected to simulated high level seismic loading of which only 28 are considered in the following. The remaining five are excluded due to insufficient data or for involving extraneous elements, e.g., support lugs. Figures 1 and 2 show typical test configurations, and Figure 3 shows a typical targeted seismic sled acceleration time history used in the PFDR tests. For each seismic test the amplitude of the sled input time history was uniformly scaled up or down to adjust the sled load levels, and the time scale of the sled input time history was stretched and compressed to adjust the frequency of the peak acceleration of the input response spectrum to be approximately 90% of the fundamental frequency of the test configuration. Figure 4 shows the narrow-banded acceleration response spectrum for a typical targeted sled acceleration time history.

Typical test run sequences were developed for the components tests. A total of up to sixteen test runs were conducted on each of the components tested and included one low level time-history input motion to establish test response at elastic stress levels and a number of high level inputs. The high level input motion was repeated up to ten times (total) or until a through-wall crack developed in the test component. Some test were terminated due to incipient instability of the test configuration as a result of ratcheting.

The 28 components evaluated in this paper and their failure modes exhibited during the tests are identified in Table 1.

2.2 Revised ASME Code Piping Seismic Design Criteria

The 1994 Addenda to the ASME Code, Section III, provide revised seismic design criteria for Class 1, 2 and 3 piping in Code Subsections NB/NC/ND-3600, respectively. The Addenda also provide alternate criteria in Code Subsection NB-3200. For piping, the 1994 Addenda introduce the concepts of reversing and nonreversing dynamic loads. For nonreversing dynamic loads and reversing dynamic loads in combination with nonreversing dynamic loads, the previous criteria are unchanged. However, conditionally higher allowable stresses are specified for reversing dynamic loads not required to be combined with nonreversing dynamic loads. Reversing dynamic loads include seismic loads.

Revised criteria are provided for both ASME Code Service Level B Operating Basis Earthquake (OBE) and Service Level D Safe Shutdown Earthquake (SSE) loads. The revised OBE criteria are not included in this review since regulatory changes in process will essentially eliminate the OBE from design considerations.

For the SSE, the revised Subsections NB/NC/ND-3600 criteria are the same for Class 1, 2 and 3 piping and require that the Code Equation (9) primary membrane plus bending stress load combination be less than $4.5 S_m$. This criterion is similar to the previous Service Level D criterion except that: 1) the method of analysis to determine the seismic bending moments is specified, 2) the previous S_y -based allowable limit is eliminated, and 3) the previous S_m -based allowable limit of $3 S_m$ is increased by 50%. The replacement of the previous S_y -based allowable stress for Code Class 2 and 3 piping by a S_m -based allowable stress is noteworthy.

Conditions for applicable of the higher allowable stress limit require, in part, that:

1. The analysis be based on an elastic response spectrum (ERS) solution utilizing 5% damping and peak broadening of no less than $\pm 15\%$.
2. B_2 -based stresses due to weight effects be limited to $0.5 S_m$.
3. C_2 -based stress ranges due to SSE anchor motion effects be limited to $6 S_m$.
4. Average axial stresses due to SSE relative anchor motion effects be limited to S_m .
5. Ratios of the outside diameter to wall thickness be limited to 50.

6. The ratio of the dominant load driving frequency to the lowest piping system natural frequency is greater than 0.5.

The alternate piping criteria of Subsection NB-3200 of the revised criteria provide that, for the SSE, the average through-wall ratchet strain due to all simultaneously applied loads be limited to 5%, and the effective peak strain range due to all simultaneously applied loads be limited on the basis of the alternating stress intensity value at 10 cycles from the applicable Code Appendix I design fatigue curve.

2.3 Technical Bases For Revised ASME Code Criteria

Evaluations in support of the revised criteria provided in the 1994 EPRI (References 4 through 8), ASME/STGIPC³ and ARC/TCG³ reports concluded that the failure mode in piping systems due to reversing dynamic loads is ratchet-fatigue and not collapse and, consequently, that elimination of reversing dynamic loads from the ASME Code collapse criterion is justified. However, stresses due to these loads need to be limited to prevent failure due to ratcheting and/or fatigue. Thus, the limitation of the average through-wall ratchet strain as specified in revised Subsection NB-3200 is intended to provide that ratcheting effects on low cycle fatigue are negligible. In turn, empirical studies are claimed to demonstrate that the increased allowable stress limits in revised Subsection NB/NC/ND-3600 also provide that the ratchet strain limit of revised Subsection NB-3200 is satisfied.

The STGIPC and TCG evaluations were based primarily on the load margins (denoted by F_m , in the STGIPC report) demonstrated during PFDR component testing. The load margin for a given set of Code design criteria is defined as the ratio of the level of the seismic load (a_f) to cause a through-wall crack in the piping system during one application of the load to the level of the load permitted under the Code criteria (a_c). For the PFDR tests, F_m was determined assuming the fatigue failure stress was inversely proportional to $\sqrt{N}$ where N is the number of stress cycles. Load margins as evaluated by the STGIPC/TCG, ignoring weight and pressure stresses, are provided in the test load margin column of Table 1. The lowest margin of 4.2 was obtained in Test 36.

Subsequently in response to issues identified by the NRC members of the STGIPC, this margin was reduced to approximately 2.1. This reduction was based on evaluations utilizing nonlinear material models and the REMS computer code to evaluate frequency ratio (R_w) effects on margins; where R_w is the ratio of the frequency (f) of the peak acceleration of the response spectrum to the fundamental frequency (f_n) of the test configuration. Issues identified and evaluated included: 1) R_w effects on margins (most of the PFDR tests were conducted at R_w ratios near 0.9), 2) the use of broadened instead of unbroadened input spectra in the evaluations, and 3) pressure stress adjustments. Issues identified and not evaluated included: 1) modeling concerns, and 2) temperature adjustments. Consideration of some of these unevaluated issues or the results of some evaluations which were ignored could have resulted in a margin of 1.7 which was less than the targeted value of 2.

³ The ASME/STGIPC and ARC/TCG reports are of limited distribution.

A typical margin vs. frequency ratio plot for Test 36 obtained during the STGIPC/TCG evaluations is shown in Figure 5.

2.4 ETEC Reviews of STGIPC/TCG Evaluations

Issues identified during ETEC reviews of the STGIPC/TCG evaluations confirmed the previously identified concerns and included a number of new concerns. The results of the reviews are included in References 2 and 9.

Essentially, the reviews found, in part, insufficient support for: 1) the elimination of consideration of the collapse failure mode under reversing dynamic loads, 2) the Code Subsection NB-3200 criteria, 3) a load based margin definition and the level of an acceptable margin, 4) design loads for supports based on the revised criteria, 5) the exclusive use of near resonance testing in the PFDR program, 6) the empirical basis for the revised NB/NC/ND-3600 criteria, 7) the extrapolation of the PFDR essentially SDOF component test results to MDOF piping systems and 8) the lack of consideration of the implications of the revised Code Section III criteria for the Code Section XI requirements.

In addition, based on margin reduction factors developed from: 1) the STGIPC/TCG REMS nonlinear evaluations of frequency ratio effects on margins, and 2) consideration of temperature effects, load margins less than 1.7 were obtained. A margin of 0.91 was obtained for Test 11 (see Table 1) and even lower margins would be obtained if additional adjustments are considered for ASME Code minimum geometries and actual vs. Code minimum material properties (Reference 2).

The ETEC reviews concluded that further evaluations of the revised seismic design criteria would require an independent assessment of frequency ratio effects on margins as well as a technical procedure for developing acceptable margins.

3. RECENT ETEC ACTIVITIES

Since the 23rd WRSB, ETEC has been involved in supporting: 1) the development of a procedure for establishing appropriate seismic margins, 2) an independent assessment of frequency effects on seismic margins, and 3) interactions with the ASME Special Working Group - Seismic Rules (SWG-SR). Activities in these areas are reported in the following.

3.1 Establishing Appropriate Seismic Margins

Reference 10 presents the framework of a procedure for establishing appropriate seismic margins based on considerations of maintaining current seismic probabilistic risk assessment (PRA) evaluation methods. Specifically, margins are established such that piping will not control the plant High-Confidence-Low-Probability-of-Failure (HCLPF) seismic capacity (as currently assumed), and consequently, the current seismic PRA methodology can be maintained. The approach is based on the assumption that the probability of failure of a segment of a piping system be limited to about 0.1% of the plant HCLPF seismic capacity level.

Reference 10 estimates that for 1% to 5% probability of failure capacities, the required seismic capacity margin should range from about 2.0 at 1% non-exceedance (NEP) capacity to 3.0 at the 5% NEP capacity.

In contrast to the preceding, based on other considerations, it might be argued that from an ASME Code viewpoint, where the concern for Service Level D Loadings is the maintenance of pressure boundary integrity, a margin of between 1 and 2, but closer to 1 (perhaps 1.33), may be acceptable. This margin should be based on ASME Code minimum geometries and minimum material properties.

Clearly, however, this ASME Code margin, may not be acceptable to assure operability of in-line or other equipment during or subsequent to the SSE.

3.2 Independent Assessment of Frequency Effects on Seismic Margins

The California Institute of Technology (CIT or Cal Tech) is providing assistance to ETEC in performing an independent assessment of frequency effects on seismic margins.

Similar to the STGIPC/TCG evaluations, the assessment is based on: 1) the development of a calibrated model for the PFDR Test 36, 2) the use of this model to develop margins for different types of input, and 3) the extension of these evaluations to other PFDR component tests. The CIT evaluation procedure is shown in the flow chart in Figure 6 and involves a number of computer code developed by Cal Tech.

The idealized SDOF model developed from the physical test configuration (Figure 7) of Test 36 is shown in Figure 8. The equation of motion for this model is given in the usual notation by:

$$m\ddot{\theta} + k_{p\delta}\theta + f_r(\theta, \dot{\theta}) = -c_f \ddot{y}_2 - f_e \quad (1)$$

where

- m = Generalized mass
- $k_{p\delta}$ = P- δ stiffness due to gravity
- f_r = Nonlinear restoring moment
- c_f = Excitation participation factor
- f_e = Eccentric moment due to mass offsets
- θ = Rotational angle
- = $(y_4 - y_2) / (l_3 + l_4)$

Rearranging Equation (1), we obtain

$$f_r(\theta, \dot{\theta}) = -m\ddot{\theta} - k_{ps}\theta - c_f \ddot{y}_2 - f_e \quad (2)$$

and noting that all terms on the right hand side of Equation (2) are known from the physical configuration of the test or were measured during the test, the evolution of the hysteretic restoring moment during the test can be obtained. This process is performed by the CIT Hysteresis Loop Identification (HLI) computer program to obtain moment vs. rotation angle plots as shown in Figure 9.

The HLI output is used to obtain initial values for the system identification program, Parameter Identification using the Parallel-distributed-Element Systems (PIPES).

In the following, we assume

$$f_r(\theta, \dot{\theta}) = c\dot{\theta} + k_l\theta + h(\theta, \dot{\theta}) \quad (3)$$

where

c = Viscous damping coefficient

k_l = Postyielding stiffness

$h(\theta, \dot{\theta})$ = Hysteretic restoring moment

(See Figure 10)

Hysteretic model of this type were developed in Reference 11 and later extended in References 12 and 13.

PIPES obtains an optimum set of characterization parameters for the nonlinear restoring moment $f_r(\theta, \dot{\theta})$ over a specified domain based on a prescribed minimization procedure. PIPES requires the initial prescription of a "skeleton curve" type for the hysteretic model (see References 11 through 13). This prescription can also be obtained from the HLI results. The minimization procedure involves the difference between the measured and the simulated responses. Comparisons of these responses are provided in Figures 11 and 12 for Test 36, Run 8. Similar comparisons are provided in Figures 13 and 14 for Test 40, Run 4 (see Figure 2).

Based on the optimized model developed by PIPES, and assuming that peak strains in the failure zone are proportional to the rotation (θ), a "calibration" factor is determined based on the time of failure during the test run and an assume fatigue evaluation procedure. The CIT evaluations are based on the sequential cycle counting procedure

and ASME Code fatigue failure data correlations for the materials considered. The evaluations are based on the measured test input and performed using the Response Simulation and Fatigue Analysis (RSFA) computer program. To date a calibrated model has been developed for Test 36. A similar model for Test 40 is not possible since the failure mode for Test 40 was ratchet buckling and not fatigue ratcheting.

The calibrated model for Test 36 is utilized in subsequent margin evaluations. Two input considered to date in these evaluations are the originally specified (required) PFDR input (as opposed to the PFDR test input) and an NRC provided input consistent with the Regulatory Guide (RG) 1.60 spectrum.

For each of these inputs, a "margin spectrum" is developed using the RSFA computer program over the range of frequency ratios $0.5 < R_w < 4.0$.

In the following interim evaluations, eccentric moments effects are neglected by setting $f_e = 0$. Accordingly, Equation (1) can be rewritten as indicated in Figure 15. Equation (d) of Figure 15 indicates that for each input $a(t)$, a family of margin spectra can be developed based on a single parameter α_w which accounts for P- δ stiffness effects.

Figures 16 and 17 show these families of margin spectra for the required PFDR and the RG 1.60 inputs, respectively. These spectra are significantly different from the STGIPC/TCG spectrum in Figure 5. All of the spectra indicate low margins for $0.5 < R_w < 1.0$. However, in this range, the minimum Cal Tech margin (~ 1.0) is lower than the minimum STGIPC/TCG margin (~ 2.0). Differing values of the ASME Code B_2 index for the Test 36 tee component were used in the evaluations (1.0 vs. 2.5), but do not account for the difference. Also, for $2.0 < R_w < 4.0$ significantly higher margins were obtained in the Cal Tech evaluations (> 5.0) than in the STGIPC/TCG evaluations (< 2.5).

These CIT margin spectra appear to indicate that fatigue type failure is of concern ($F_{mr} < 2.0$) for $R_w < 0.8$, but not for $R_w > 1.0$ ($F_{mr} > 4.0$). However, inspections of the responses (rotation) for $R_w > 1.5$ confirmed a concern regarding the effects of large response (see Figure 18).

This concern is demonstrated in Figure 19 which shows, for any value of R_w , the sudden appearance of large responses (as indicated by the "cliff" feature in the CUF profile) with increasing load levels. The load level at which these large responses appears decreases with increasing R_w . For $R_w > 2.0$, fatigue failure results from the large strains during these large rotations.

Accordingly, the concept of a ductility based load margin $F_{m\mu}$ was introduced and evaluated. Similar to the previous fatigue based load margin (F_{mr}), $F_{m\mu}$ is defined as the ratio of a_μ and a_c where a_μ is the level of the load to achieve a specified ductility ratio (μ) and a_c is the same as previously defined. The ductility ratio is defined as the ratio of the maximum rotation, θ_{max} , to the rotation at yield, θ_y . For tests that did not failure in the fatigue ratcheting mode, but in the ratchet buckling mode, the ductility based margin provides an appropriate measure of margin if θ_{max} is set equal to the critical rotation at the onset of buckling.

Ductility based margin spectra for Tests 36 and 40 are provided in Figures 20 and 21. For Test 36, θ_{max} was set equal to the rotation at which failure occurred in Run 8 and for Test 40 the spectra in Figure 21 include the effects of the eccentric moment f_e and α_ω or P- δ effects.

The margin spectra in Figures 20 and 21 show that, in general, ductility based margins are lower than fatigue based margins. For $0.5 < R_w < 1.0$, these margins are approximately equal with the lowest values (~ 1) occurring at $R_w = 0.5$. For $R_w > 1.0$, except for the local peaks near $R_w = 2.5$ and 4.0 , the ductility based margin values are approximately one half of the fatigue based values for Test 36; and f_e and P- δ effects lower the margin values significantly for Test 40. The local peaks at $R_w = 2.5$ and 4.0 are due to the frequency content of the PFDR input.

3.3 ASME Code SWG-SR

In September 1995 the SWG-SR was established by the ASME Code, Section III, Subgroup Design (SGD) to consider "new comments" made since the initial passage of the revised criteria. The SWG-SR has identified 15 categories of issues and has prioritized these categories into 4 classes.

Currently there are two NRC observers to the SWG-SR. ETEC supports SWG-SR activities by reporting the results of ongoing evaluations, performed to clarify some of the important issues, to the SWG-SR.

4. INTERIM CONCLUSIONS

The ongoing independent evaluations of frequency ratio effects on seismic margins indicate that the previous STGIPC/TCG fatigue based margin evaluation was too narrowly focused and provided no insight into the almost sudden appearance of large responses which increasing load levels. The ductility based margin⁴ which was developed to investigate this phenomenon quantifies this behavior.

Both the fatigue and ductility based margin spectra developed during the independent evaluations and the STGIPC/TCG fatigue margin spectrum indicate potentially unacceptable low margins for R_w near 0.5.

For $R_w > 2.0$, the independent evaluations indicate that, contrary to the results of the STGIPC/TCG evaluations, large displacement effects are more of a concern than fatigue failure. These large displacement effects include incipient collapse and buckling. Moreover, quantitative results, such as fatigue analyses, based on these large displacements are highly uncertain since: 1) the analyses for these displacements may have been extended beyond their range of applicability, 2) strain concentration effects have been ignored, and 3) the fatigue failure correlation may be less reliable for the large strains ranges being evaluated.

⁴ The introduction of the ductility based margin concept also indicates that margins based on other considerations, e.g., buckling or the onset of enhanced ratcheting, could also be developed.

Based on the preceding results, the independent assessment of frequency effects on seismic margins does not provide support for the increased Equation (9) allowable stress in the revised ASME Code, Section III, seismic design criteria for nuclear plant piping. The evaluation indicate: 1) potentially unacceptable low margins for R_w near 0.5, 2) concerns regarding large displacement effects for $R_w > 2.0$.

Similar evaluations for other PFDR test configuration, other than Tests 36 and 40, are planned to verify the applicability of the results obtained in Tests 36 and 40 to other tests.

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TABLE 1
EXTRAPOLATED LOAD MARGINS FOR PFDR TESTS
BASED ON REVISED CRITERION $B_2M/Z \leq 4.5 \text{ SM}$

PFDR COMPONENT TEST NO.	COMPONENT DESCRIPTION TYPE	MAT/SCH	TEST PRESSURE (psi)	# TO FAIL	FAILURE MODE	CYCLE CORRECTED TEST LOAD MARGIN	FREQUENCY RATIO Rw (1/fm)	REDUCTION FACTORS		
								TEMP	UNBR vs. BR & Rw	EXTRAPOLATED LOAD MARGIN
1	6 in. EL-LR	CS/80	2600	0.5	FR	14.0	0.833	0.82	0.34	3.90
2	6 in. EL-LR	CS/80	2600	4.5	FR	18.5	0.859	0.82	0.61	4.31
3	6 in. EL-LR	SS/10	400	3.5	FR	15.7	0.900	0.76	0.33	3.75
4	6 in. EL-LR	CS/40	1000	2.5	FR	14.4	0.806	0.82	0.37	3.44
5	6 in. EL-LR	CS/40	1700	3.5	FR	15.7	0.900	0.82	0.33	3.75
6	6 in. EL-LR	SS/40	1700	3.5	FR	14.2	0.900	0.76	0.44	3.27
7	6 in. EL-LR	SS/40	1000	4.5	FR	19.5	0.714	0.76	0.34	5.97
8	6 in. EL-LR	SS/40	0	5.0	NF	21.4	0.917	0.76	0.33	4.92
9	6 in. TEE (2)	SS/40	1700	1.5	FR	10.3	0.900	0.76	0.33	1.12
10	6 in. TEE (2)	SS/40	1000	2.5	FR	13.3	0.900	0.76	0.33	1.47
11	6 in. TEE (2)	SS/10	400	0.5	FR	4.5	0.862	0.76	0.30	0.91
12	6 in. TEE (2)	SS/40	1700	2.5	FR	17.0	0.875	0.76	0.30	1.62
13	6 in. EL-SR	CS/40	1000	2.5	FR	13.5	0.897	0.82	0.33	4.23
14	6 in. TEE (2)	CS/40	1700	1.5	FR	8.8	0.875	0.82	0.30	1.03
15	8x4 + 6x4 RED	SS/40	1700	5.0	FR	11.6	0.875	0.76	0.30	2.51
16	8x4 + 6x4 RED	CS/40	1700	0.5	FR	8.5	0.905	0.82	0.33	3.05
19	6 in. EL-LR	SS/40	2500	3.0	FR	15.2	0.956	0.76	0.37	3.59
30	6 in. EL-LR	SS/10	0,400	3.0	FR	6.9	0.972	0.76	0.38	2.08
31	6 in. EL-LR	SS/10	400	3.5	FR	17.2	0.875	0.76	0.30	3.74
34	6 in. PIPE	CS/40	1000	4.0	FR	9.6	0.914	0.82	0.34	3.37
35	6 in. EL-LR	CS/40	1700	5.0	FR	16.1	0.932	0.82	0.35	4.03
36	6 in. TEE (R)	CS/40	1700	0.5	FR	4.2	0.770	0.82	0.43	0.95
37	6 in. EL-LR	SS/10	0	2.0	RB	5.6	0.975	0.76	0.38	1.63
38	6 in. TEE (1)	SS/40	1700	3.6	FR	15.1	0.929	0.76	0.35	4.60
39	6 in. TEE(1)	SS/40	0	4.0	NF	16.8	0.956	0.76	0.37	5.09
40	8x4 + 6x4 RED	SS/40	0	2.0	RB	12.4	0.929	0.76	0.35	2.72
41	6 in. EL-LR	CS/40	1700	2.0	FR	14.1	0.929	0.82	0.25	3.67

NOTES TO TABLE 1

COMPONENT DESCRIPTION

EL-LR	Long radius elbow
EL-SR	Short radius elbow
TEE(2)	Tee, 2 ends fixed, branch loading
TEE (1)	Tee, 1 end fixed, branch loading
TEE (R)	Tee, run loading
RED	Opposed reducers and straight pipe
CS	Carbon Steel
SS	Stainless Steel

Test Pressure

From Reference 5

Failure Data

From Reference 5

to FAIL = Number of high level input-test runs to cause failure

NF = No failure

FR = Fatigue ratchet failure

RB = Ratchet buckling

Cycle Corrected Test Load Margin

From ASME/STGIPC report

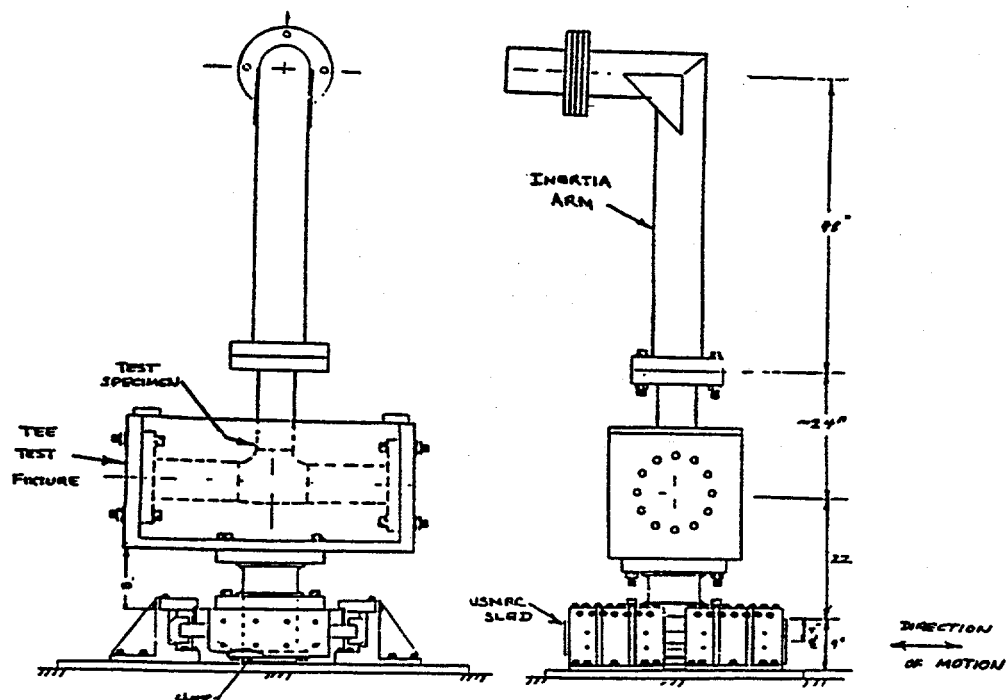


Figure 1: PFDR Test 11 Setup

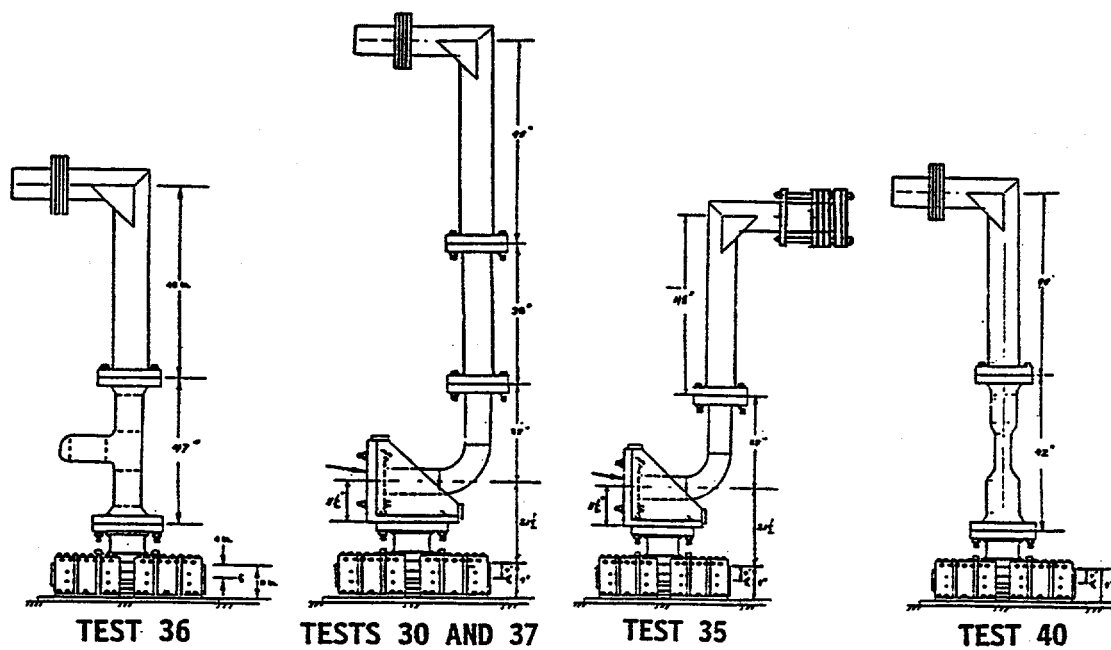


Figure 2: Other Key PFDR Test Setups

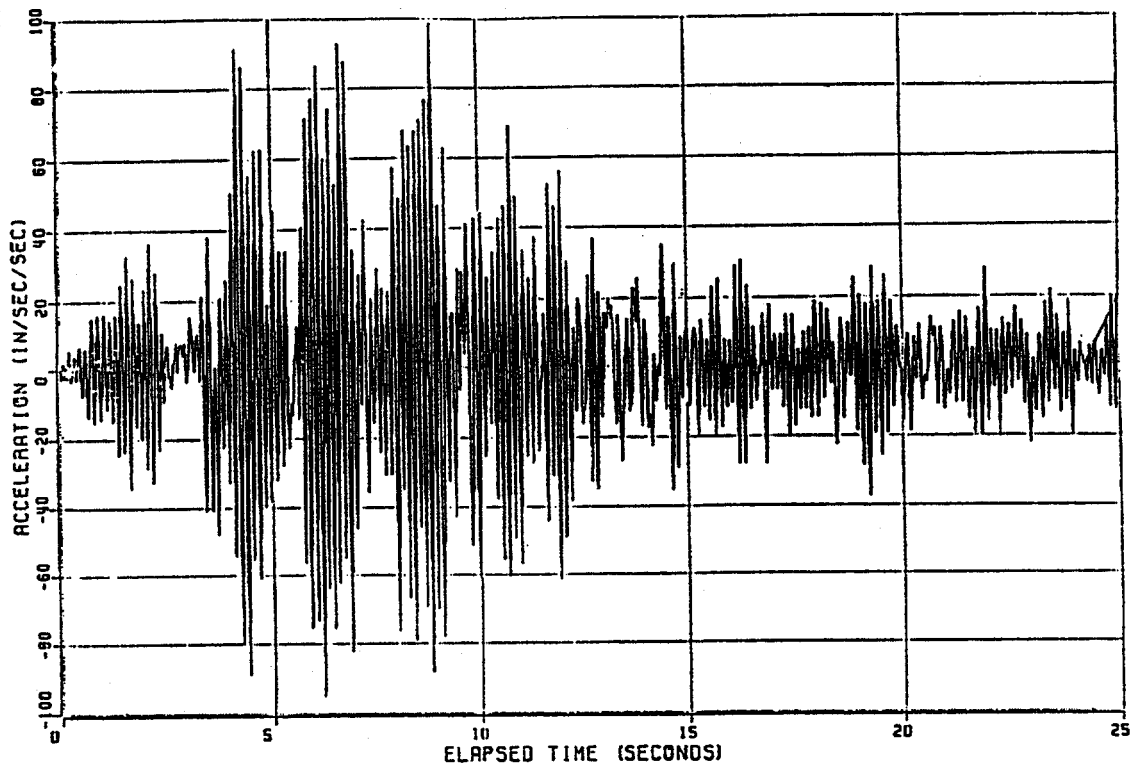


Figure 3: Typical PFDR Input Acceleration Time History

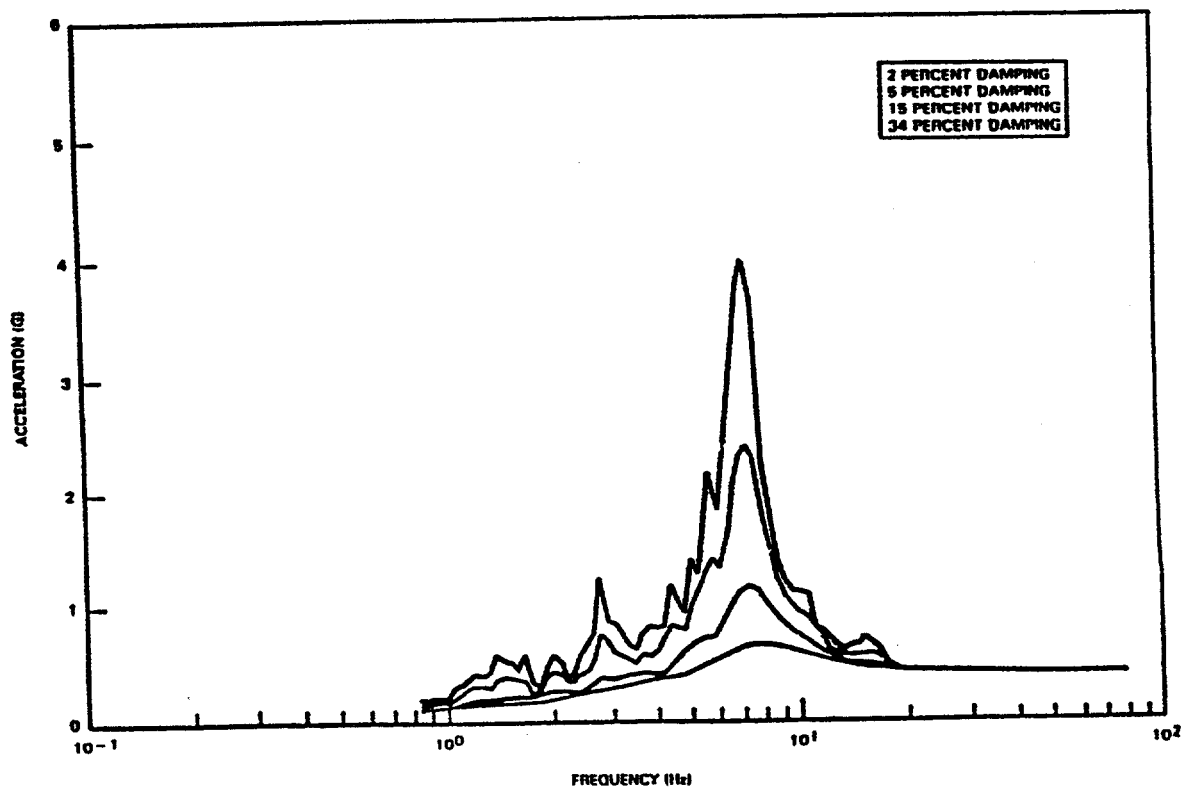
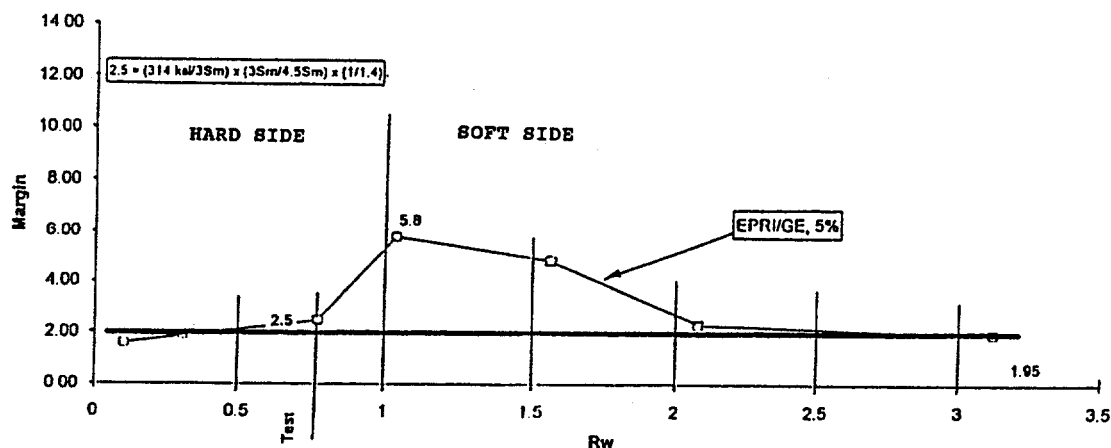


Figure 4: Response Spectra for Input Acceleration Time History

GE Reported Unbroadened Spectra, Test #38
Corrected for Cycles - Using Established Shape for Freq Variation



- $R_w = \frac{\text{FREQUENCY OF PEAK INPUT ACCELERATION}}{\text{FUNDAMENTAL ELASTIC FREQUENCY}}$

Figure 5: Typical Frequency Ratio Effect on Seismic Margin
(ASME/STGIPC & ARC/TCG Analysis)

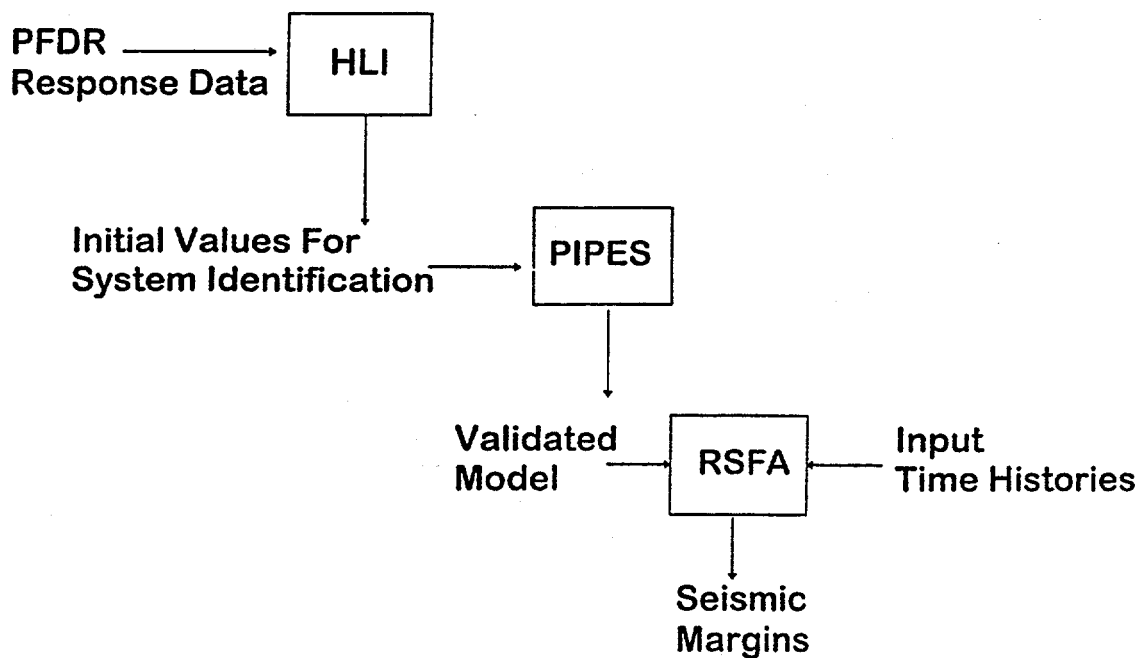


Figure 6: Cal Tech Margin Evaluation Flowchart

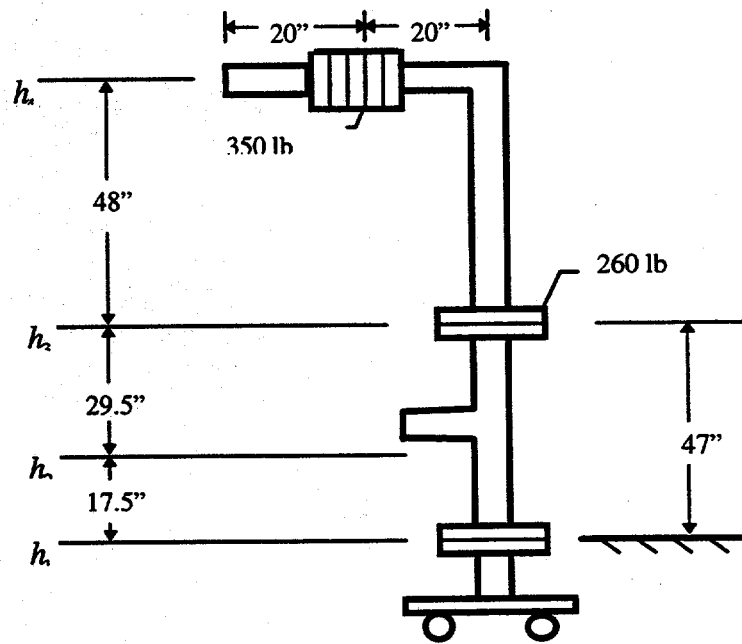


Figure 7: Physical Configuration of Test 36

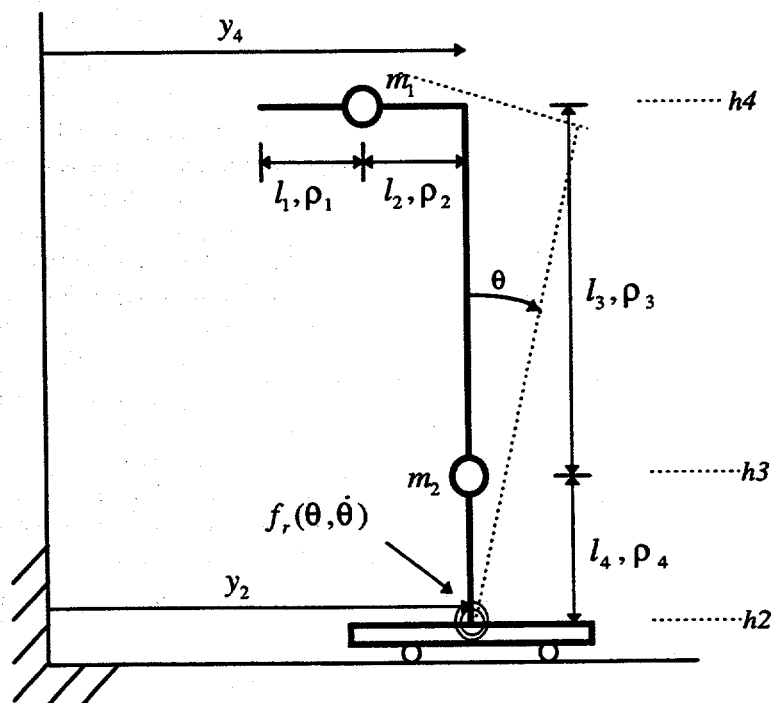


Figure 8: Idealized SDOF Model for Test 36

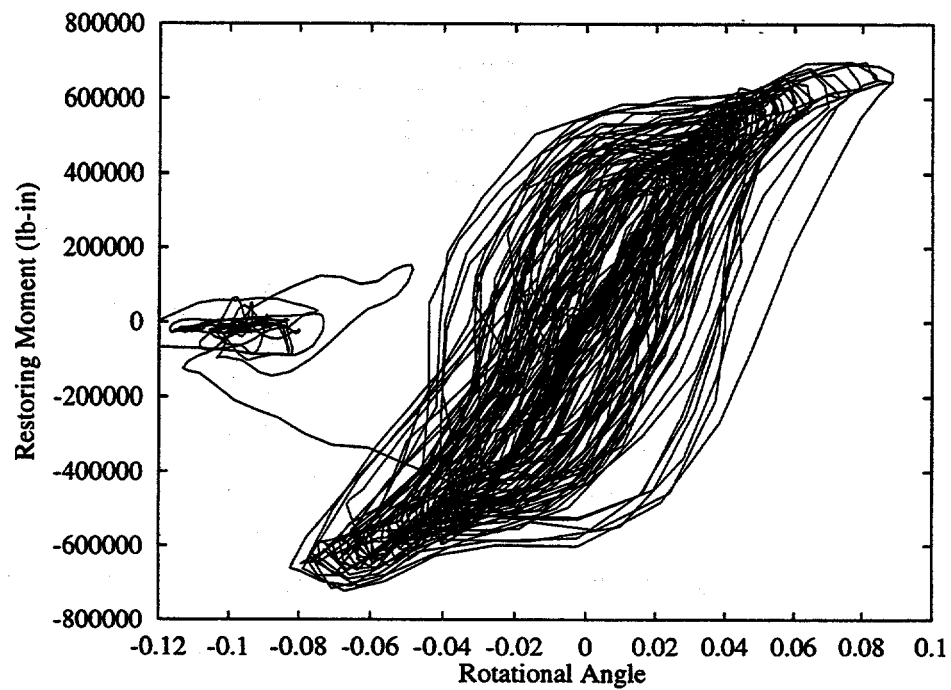


Figure 9: HLI Hysteresis Loops for Test 36, Run 8

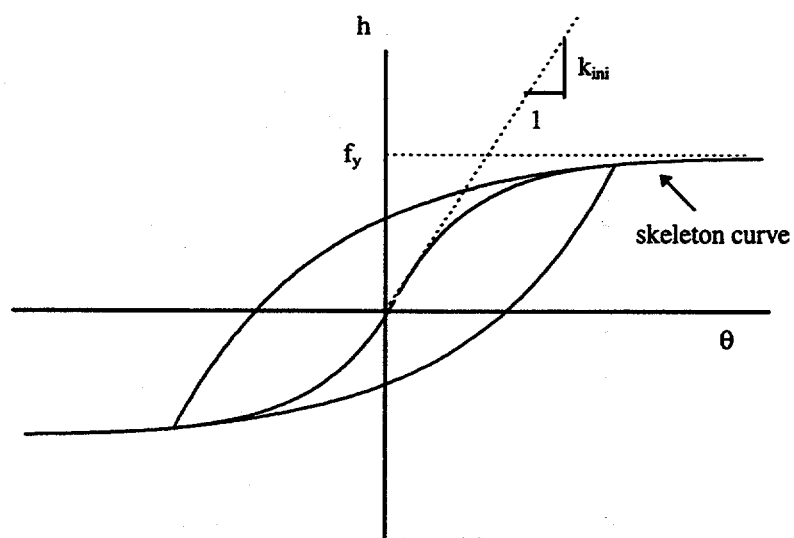


Figure 10: Schematic Hysteretic Restoring Moment

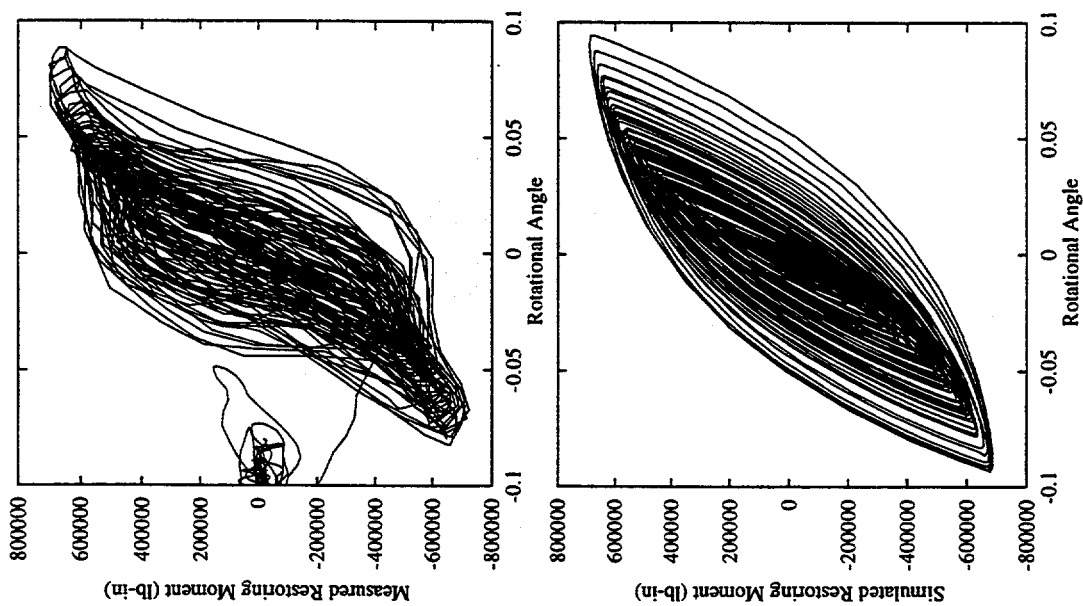


Figure 11: Comparison of Measured vs. Simulated Hysteresis Loops: Test 36, Run 8

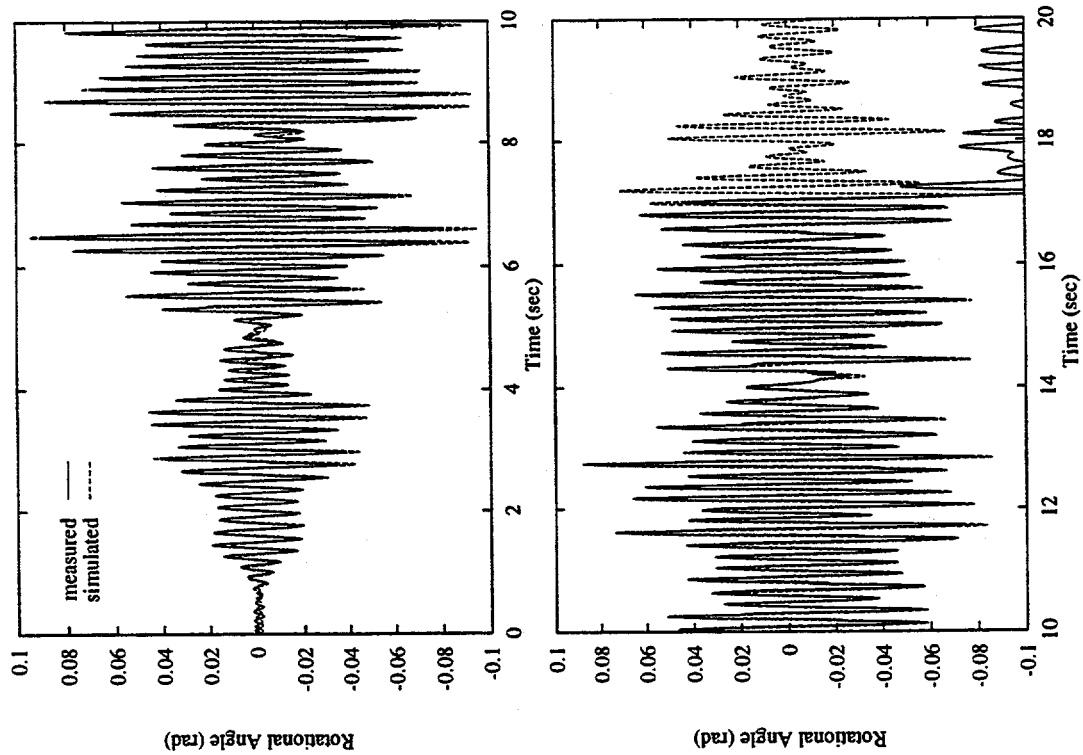


Figure 12: Comparison of Measured vs. Simulated Response: Test 36, Run 8

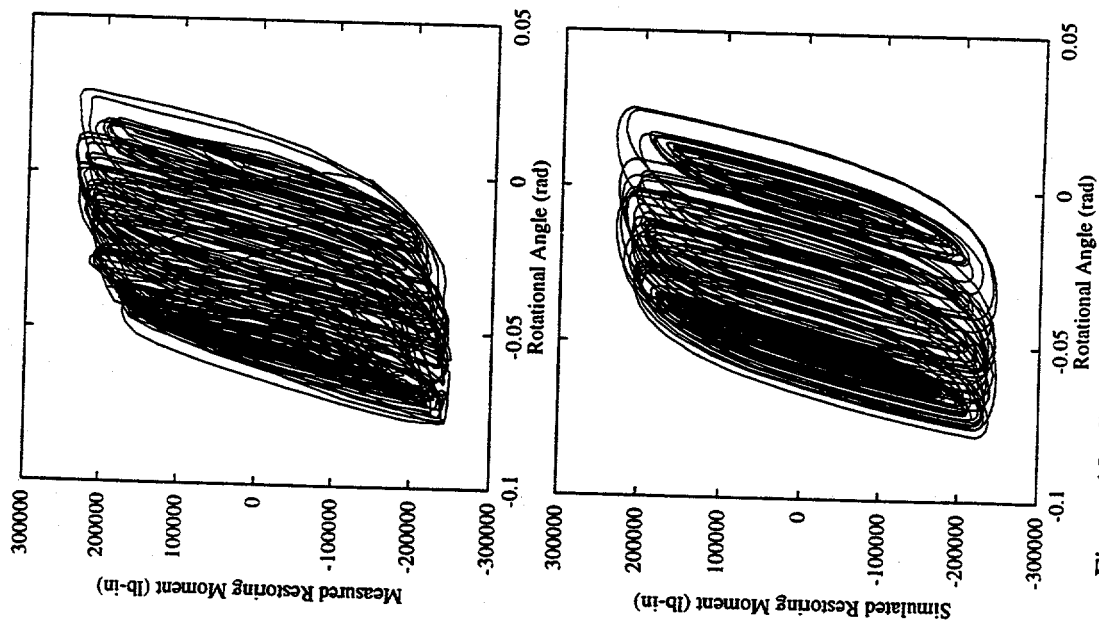


Figure 13: Comparison of Measured vs. Simulated Hysteresis Loops: Test 40, Run 4

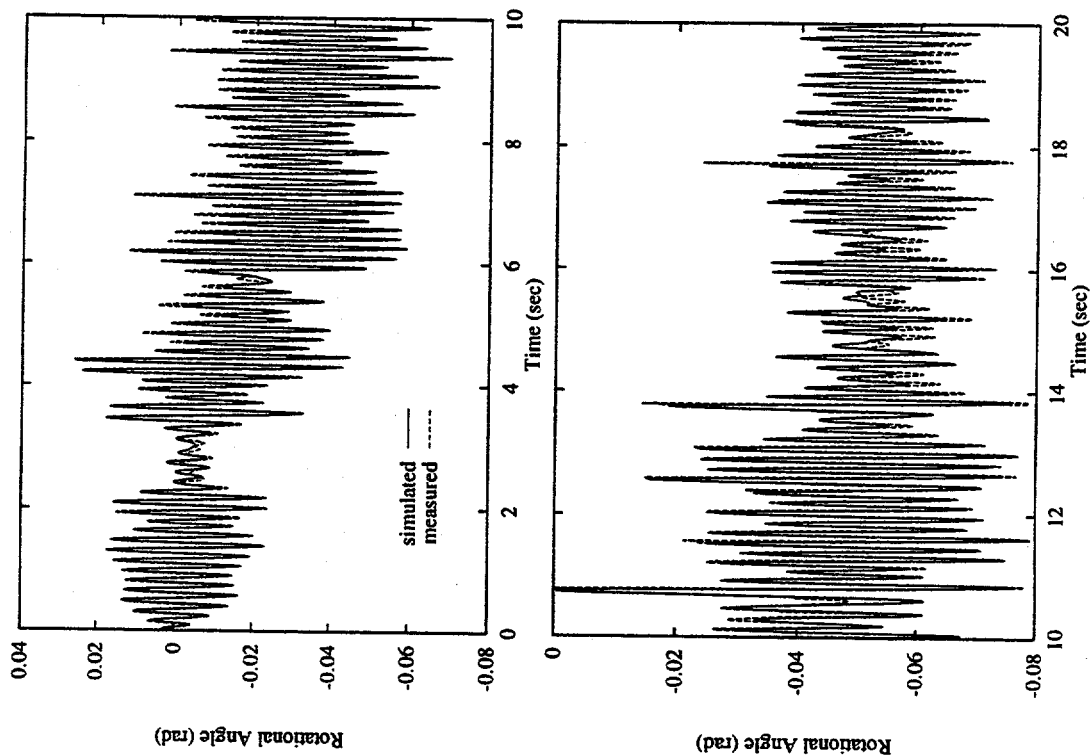


Figure 14: Comparison of Measured vs. Simulated Response: Test 40, Run 4

Equation (1) can be rewritten as

$$\ddot{\theta}(t) + 2\zeta\omega_n\dot{\theta}(t) + \alpha_w\omega_n^2\theta(t) + (1-\alpha_w)\omega_n^2z(\theta, \dot{\theta}) = c_p a(t) \quad (a)$$

where

$$\zeta = \frac{c}{2\sqrt{mk}}$$

$$\omega_n = \sqrt{\frac{k}{m}}$$

$$\alpha_w = \frac{k_{ps}}{k}$$

$$k = k_{ps} + k_{ini} + k_t$$

$$c_p = \frac{c_f}{m}$$

$$z(\theta, \dot{\theta}) = \frac{k_t\theta + h(\theta, \dot{\theta})}{k_t + k_{ini}}$$

$h(\theta, \dot{\theta})$ = Hysteretic restoring moment with linear initial stiffness k_{ini}

$a(t)$ = Excitation source, e.g., PFDR or RG 1.60 input

and all other terms are as before.

It can be shown that, for a given input $a(t)$ and all λ :

$$\left. \begin{aligned} a_f &= a_f(\omega_n, \alpha_w, c_p) \\ a_f(\omega_n, \alpha_w, \lambda c_p) &= \frac{1}{\lambda} a_f(\omega_n, \alpha_w, c_p) \end{aligned} \right\} \quad (b)$$

$$\left. \begin{aligned} a_c &= a_c(\omega_n, c_p) \\ a_c(\omega_n, \lambda c_p) &= \frac{1}{\lambda} a_c(\omega_n, c_p) \end{aligned} \right\} \quad (c)$$

but that

$$\left. \begin{aligned} Fm_r &= Fm_r(\omega_n, \alpha_w) \\ \text{or } Fm_r &= Fm_r(R_w, \alpha_w) \end{aligned} \right\} \quad (d)$$

where R_w is the previously defined frequency ratio.

Figure 15: Governing Differential Equation and Functional Relationships

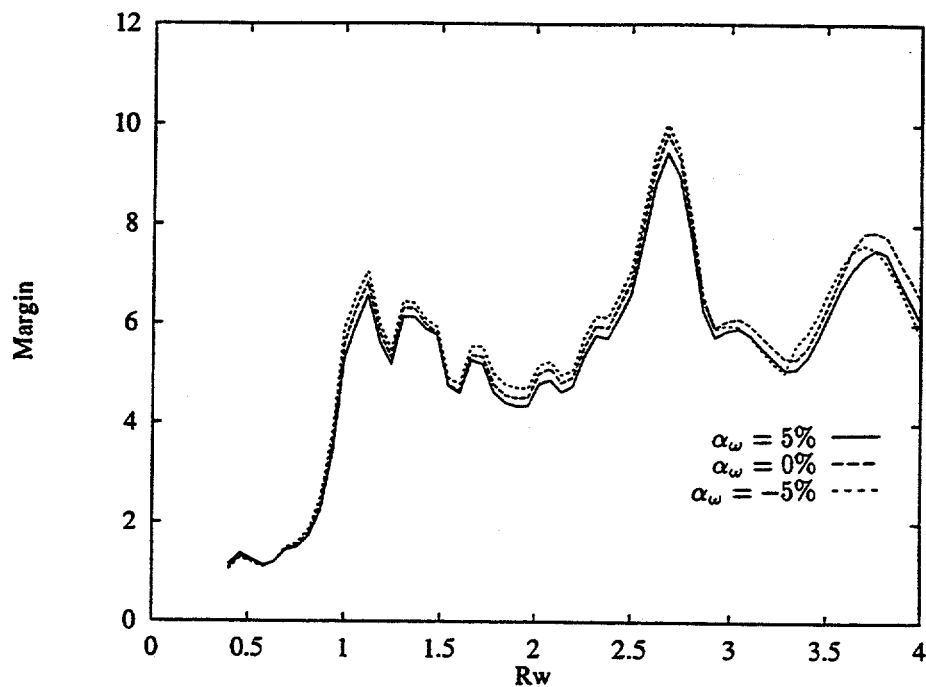


Figure 16: Margin Spectra for PFDR Input, Test 36 Model

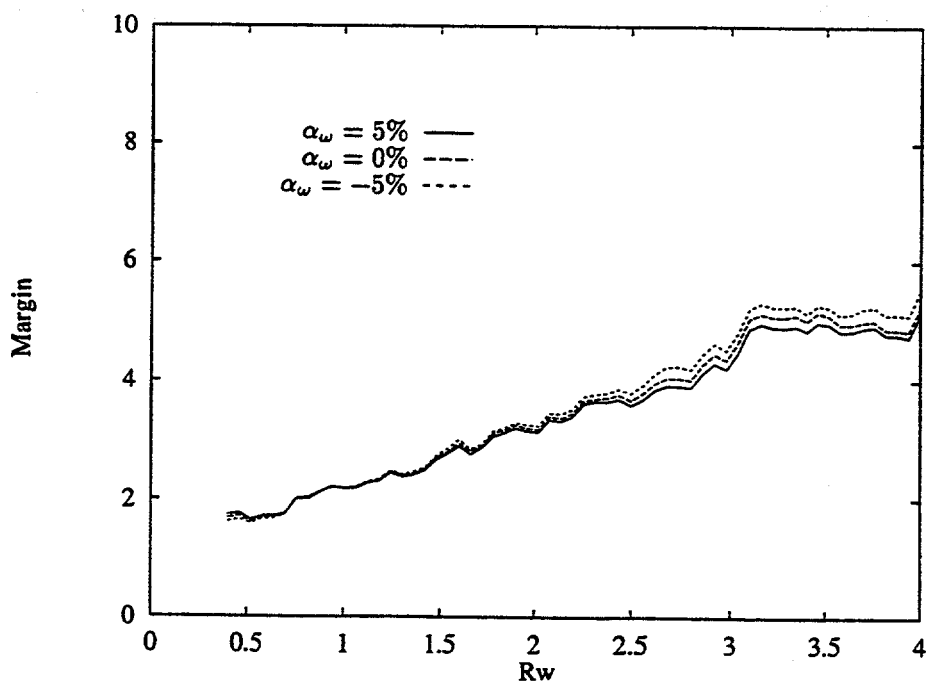


Figure 17: Margin Spectra for RG 1.60 Input, Test 36 Model

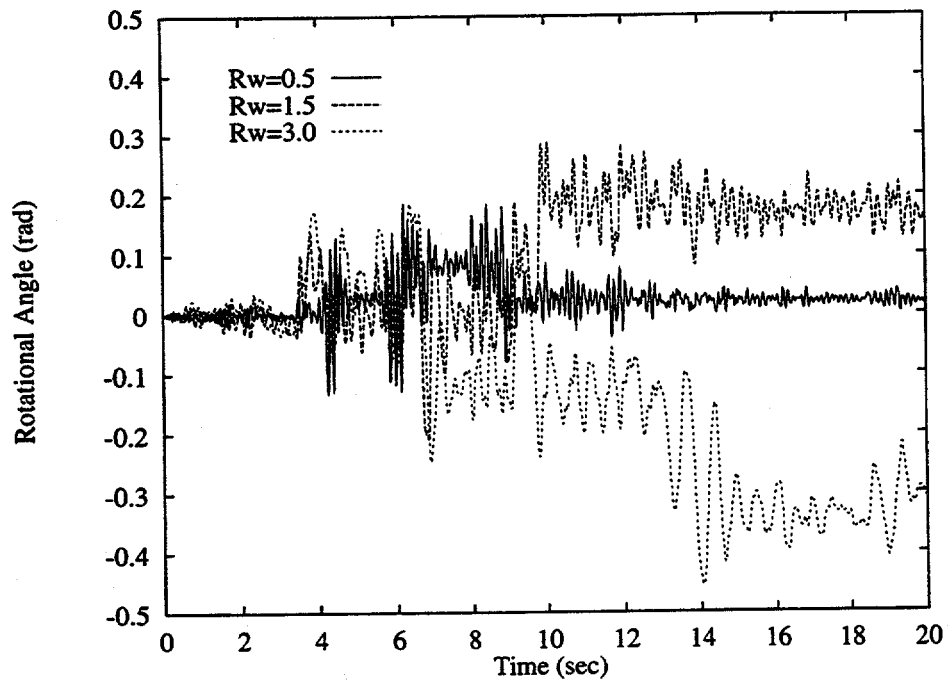


Figure 18: Response of Test 36 Model to PFDR Input
($\alpha_w = -3\%$, $R_w = 0.5, 1.5, 3.0$)

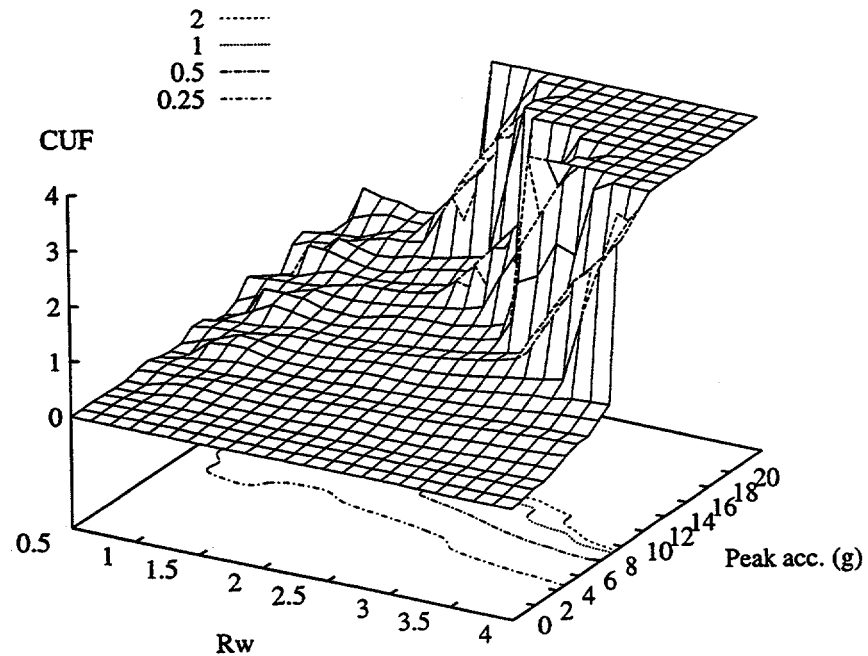


Figure 19: CUF Profile for Test 36 Model and PFDR Input ($\alpha_w = -5\%$)

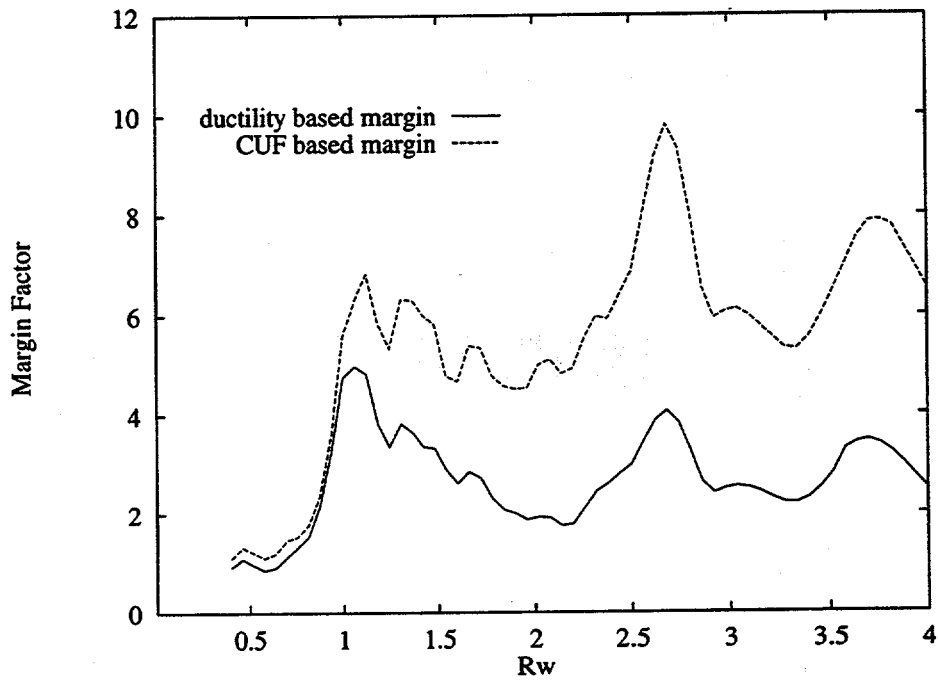


Figure 20: Fatigue Based and Ductility Based Margin Spectra, Test 36 Model, PFDR Input, $f_e=0$

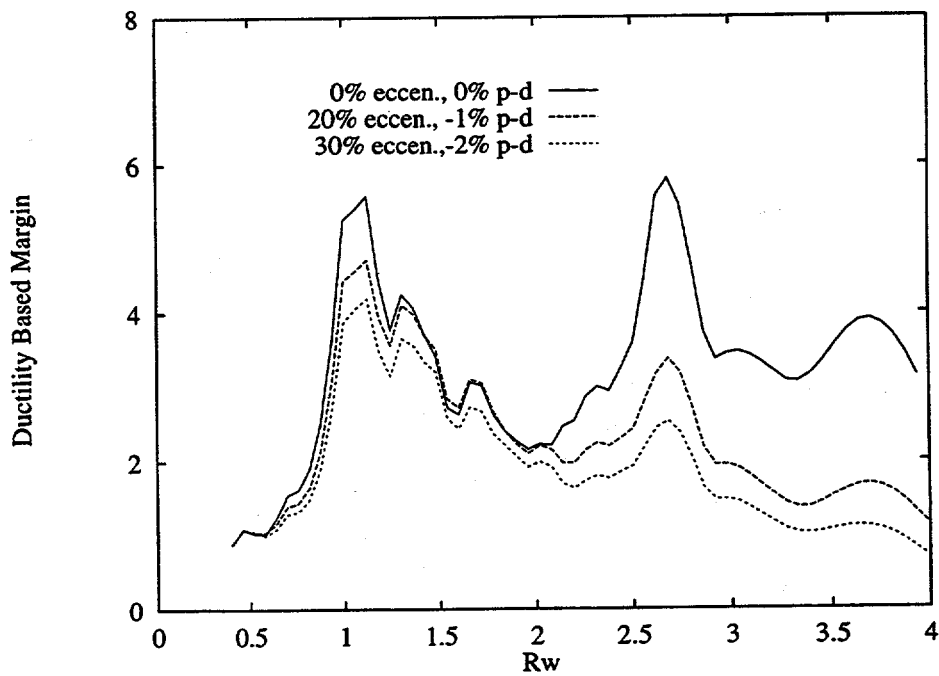
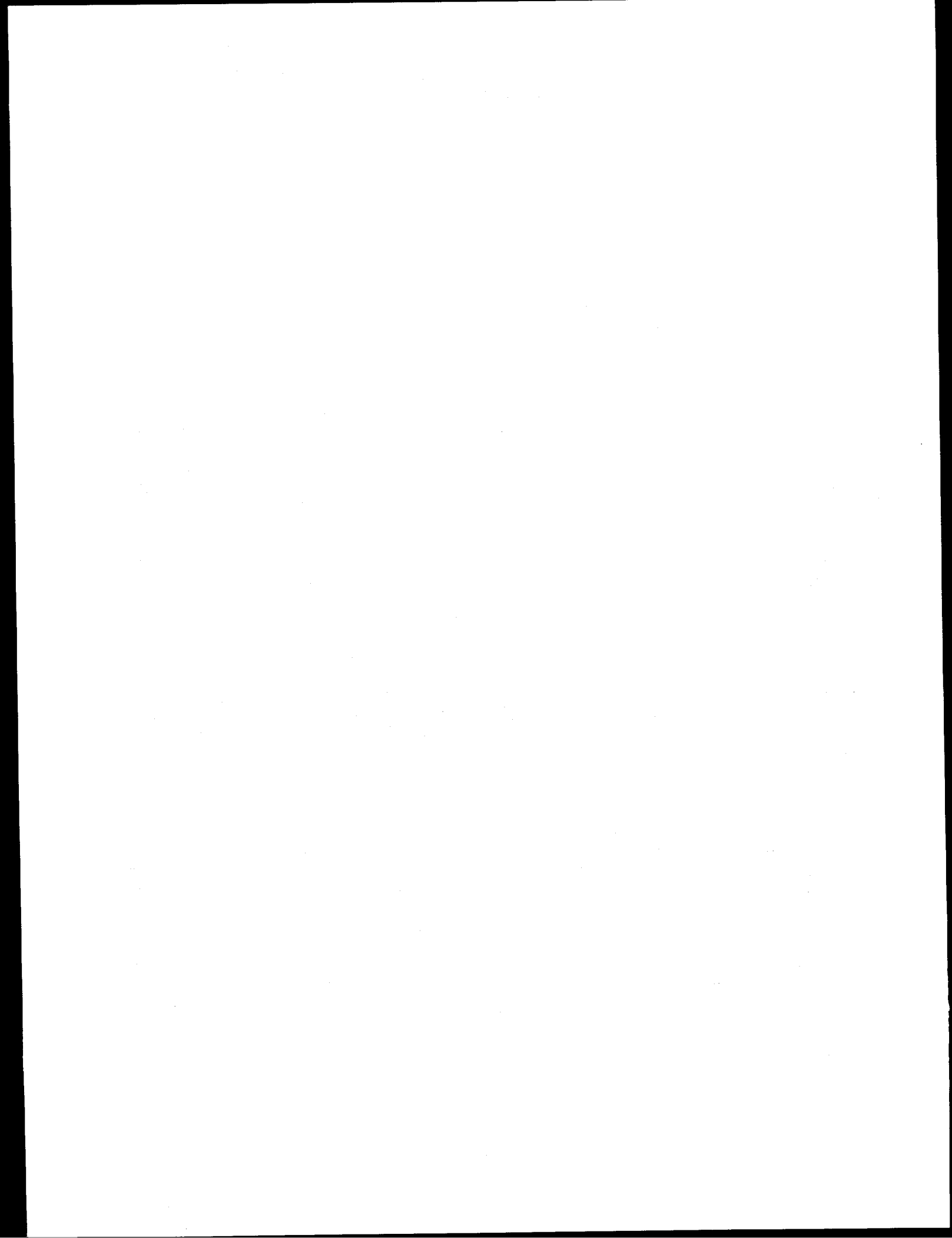


Figure 21: Ductility Based Margin Spectra, Test 40 Model, PFDR Input



STRUCTURAL AGING PROGRAM - A SUMMARY OF ACTIVITIES, RESULTS, AND CONCLUSIONS

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ABSTRACT

Research has been conducted by the Oak Ridge National Laboratory to address aging management of nuclear power plant concrete structures. The purpose was to identify potential structural safety issues and acceptance criteria for use in continued service assessments. Primary program accomplishments have included formulation of a Structural Materials Information Center that contains data and information on the time variation of material properties under the influence of pertinent environmental stressors and aging factors for 144 materials, an aging assessment methodology to identify critical structures and degradation factors that can potentially impact their performance, guidelines and evaluation criteria for use in condition assessments of reinforced concrete structures, and a reliability-based methodology for current condition assessments and estimations of future performance of reinforced concrete nuclear power plant structures. In addition, the Structural Aging Program conducted in-depth evaluations of several nondestructive evaluation and repair-related technologies to develop guidance on their applicability.

1. INTRODUCTION

Nuclear power provides in excess of 20 percent of the net electricity generation in the United States (U.S.). By the end of this decade, over 60 of the 109 commercial nuclear power plants (NPPs) that had been licensed for commercial operation in the U.S. (as of March 1995) will be more than 20 years old, with some nearing the end of their 40-year operating license.¹ Faced with the prospect of having to replace the lost generating capacity from other sources and the potential for substantial shutdown and decommissioning costs, many utilities are expected to seek extensions to their initial plant operating licenses.

The importance of nuclear power and the necessity for ensuring continued satisfactory operation is thus clearly established. One of the primary factors that could affect the continued operation and development of nuclear power relates to aging of the plants and its potential impact on performance.² Nuclear power plants are designed, built, and operated to standards that aim to reduce the releases of radioactive materials to levels as low as reasonably achievable. Nuclear power plants, however, involve complex engineering structures and components operating in demanding environments that potentially can challenge the high level of safety (i.e., safety margins) required throughout the operating life of the plant.* It is necessary that safety issues related to plant aging and continuing the service of the

* Continued performance of NPP structures is also of interest during decommissioning.³ By delaying the complete dismantling and removal of reactors and associated buildings for periods ranging from several decades to about 100 years after the plant has ceased operation, the dismantling process can be facilitated due to the anticipated reduction of radioactivity levels. Throughout the decommissioning period, however, the safety-related civil structures are expected to continue to meet several of their intended functions (e.g., leaktightness and shielding).

nuclear plants be resolved through development of sound scientific and engineering understanding. One specific area that has been noted is the aging behavior of structural materials — particularly those in pressure-retaining structures.² Although major mechanical and electrical equipment items in a plant could be replaced, if necessary, replacement of many of the safety-related concrete structural components would be economically unfeasible. Approval for a continuation of service must be supported by evidence that these structures will continue to be capable of withstanding potential future extreme events.

2. SAFETY-RELATED CONCRETE STRUCTURES

2.1 General Description

All commercial NPPs contain concrete structures whose performance and function are necessary for protection of the safety of plant operating personnel and the general public, and the environment. Typical safety-related functions that the concrete structures provide include foundation, support, biological shielding, containment, and protection against internal and external hazards. Each boiling-water reactor (BWR) or pressurized-water reactor (PWR) unit in the U.S. is housed within a much larger metal or concrete containment.

Concrete containments for PWRs are fabricated from reinforced concrete, that in some cases may be post-tensioned. They all enclose the primary circuit that includes the reactor pressure vessel, steam generators, etc. Three general categories of PWR containments exist: large dry, ice condenser, and subatmospheric. The large dry containment is designed to have a capacity to contain the energy of the entire volume of primary coolant fluid in the unlikely event of a loss-of-coolant-accident (LOCA). The ice condenser containments channel the steam resulting from a LOCA through ice beds to reduce the pressure buildup and thus the containment volume and pressure requirements. The subatmospheric containments are designed so that a slightly negative pressure is maintained in the containment to reduce the volume requirements.

Although the majority of BWR plants utilize a steel containment vessel, a number of units utilize either a prestressed- or reinforced-concrete containment. With only one exception, all BWR plants in the United States that utilize a steel containment have reinforced concrete structures that serve as secondary containments or reactor buildings and provide support and shielding functions for the primary containment. The containments are divided into two main compartments — wetwell and drywell. After a LOCA, the air and steam in the drywell are forced through a number of downcomers to a pool in the wetwell, where the steam condenses. Water spray systems are provided and the auxiliary systems are generally housed in the secondary containment.

A myriad of other concrete-based structures are also contained as part of a light-water reactor (LWR) plant (e.g., reactor pedestal/support, intake structures, and primary shield wall). References 4-7 provide more detailed descriptions of the safety-related concrete structures.

2.2 Materials of Construction

Nuclear safety-related concrete structures are composed of several constituents that, in concert, perform multiple functions (e.g., load-carrying capacity, radiation shielding, and leaktightness). Primarily, these constituents include concrete, conventional steel reinforcement, prestressing steel, and steel liner materials. The concretes typically consist of a moderate heat of hydration cement, fine aggregates (sand), water, various admixtures for improving properties or performance of the concrete, and either normal-weight or heavyweight coarse aggregate. Both the water and aggregate materials are normally obtained from local sources and are subjected to material characterization prior to use. Mild steel reinforcement is used primarily to provide tensile and shear load resistance/transfer and consists of plain carbon steel bar stock with deformations (lugs or protrusions) on the surface. Minimum tensile yield strengths generally range from 270 to 415 MPa. The post-tensioning system consists of prestressing tendons that are installed, tensioned, and then anchored to the hardened concrete forming the structure. The prestressing tendons provide primary resistance to tensile loadings and also improve resistance to shear forces that might develop during earthquake loadings. Three major categories of prestressing system exist depending on the type of tendon utilized — wire, bar, or strand. Minimum ultimate strengths of prestressing tendons generally range

from 2,000 to 10,000 kN. The tendons are installed within preplaced ducts in the containment structure and are post-tensioned from one or both ends after the concrete has achieved sufficient strength. After tensioning, the tendons are anchored by buttonheads, nuts, or wedges, depending on the tendon type. With the exception of two plants (Robinson 2 and Three Mile Island 2), corrosion protection is provided by filling the ducts with wax or corrosion-inhibiting grease. Metallic liners are provided on the inside surface of the containments to provide a barrier against leakage. A typical liner is composed of steel plate stock less than 13-mm-thick, joined by welding, and anchored to the concrete by studs, structural steel shapes, or other steel products. The PWR containments and drywell portions of BWR containments are typically lined with carbon steel plate. The liner of the wetwell of BWR containments, as well as the LWR fuel pool structures, typically consists of stainless steel plates. Certain LWR facilities have used carbon steel plates clad with stainless steel for the liner. Although the liner's primary function is to provide a leaktight barrier, it also can act as part of the formwork during concrete placement.

2.3 Aging and Durability Considerations

Reinforced concrete structures at NPPs historically have been designed in accordance with national consensus codes and standards in force at the time (e.g., ACI 318).⁸ The rules in these documents were developed over the years by experienced people and are based on the knowledge that was acquired in testing laboratories and supplemented by field experience. Design principles have been dominated by analytical determinations based on strength principles. Durability considerations require that the time element be factored into the design of reinforced concrete structures. Associated with the design specifications developed for concrete structures in conformance with these calculations was a certain level of durability (e.g., minimum concrete cover requirements to protect embedded steel reinforcement under different anticipated environmental conditions). Although the vast majority of reinforced concrete structures, and particularly those associated with NPPs, have met and continue to meet their functional and performance requirements, several instances can be cited where these structures have exhibited degradation. These degradation instances have generally been due to the effects of environmental stressors.

Primary mechanisms or factors that can produce premature deterioration of concrete structures include those that impact either the concrete or reinforcing steel materials. Degradation of concrete can be caused by adverse performance of either its cement-paste matrix or aggregate materials under chemical or physical attack. Chemical attack may occur in several forms: efflorescence or leaching, sulfate attack, attack by acids and bases, salt crystallization, and alkali-aggregate reactions. Physical attack mechanisms for concrete include freeze/thaw cycling, thermal exposure/thermal cycling, abrasion/erosion/cavitation, irradiation, and fatigue or vibration. Degradation of mild steel reinforcing materials can occur as a result of corrosion, irradiation, elevated temperature, or fatigue effects. Prestressing materials are susceptible to the same degradation mechanisms as mild steel reinforcement, plus loss of prestressing force, primarily due to tendon relaxation and concrete creep and shrinkage.

2.4 Operating Experience

The performance of NPP concrete structures has been very good. Long-term performance evaluations of these structures indicate that they can continue to meet their functional and performance requirements over a period significantly longer than their initial design life. However, there have been several isolated instances that if not remedied could challenge the capacity of the concrete containment and other safety-related concrete structures to meet future functional and performance requirements. Some of the aging concerns that have been identified to date include inaccessibility of reinforced concrete basemats for inspection to detect potential degradation resulting from mechanisms such as leaching or sulfate attack, corrosion of steel in water-intake structures, and corrosion of embedded portions of the steel pressure boundary (liner) due to a breakdown of the seal at the floor-to-liner interface.

In general, many of the reported degradation instances associated with NPP concrete structures occurred early in the life of the structures and primarily have been attributed to construction/design deficiencies, improper material selection, or environmental effects. Although the vast majority of these structures will continue to meet their functional and performance requirements during their service period, it is reasonable to assume that there will be isolated incidents where the structures may not exhibit the desired durability without some form of intervention.

Some general observations derived from one organization's condition surveys of concrete structures in several NPPs in both the U.S. and Europe were that virtually all NPPs have experienced cracking of the concrete structures that exceeded typical acceptance criteria for width and length, numerous plants had groundwater intrusion occurring through the power block or other subsurface structures, few plants have a comprehensive program for conducting periodic inspections of the concrete structures, and aging concerns exist for subsurface concrete structures as their physical condition cannot be easily verified.⁹ Examples of some of the specific problems that have occurred due to age-related degradation include corrosion of steel reinforcement in water intake structures, corrosion of post-tensioning tendon wires, leaching of tendon gallery concrete, low prestressing forces, leakage of corrosion inhibitor from tendon sheaths into the concrete, freeze/thaw damage, and corrosion of containment metallic liners.¹⁰⁻¹³

3. STRUCTURAL AGING PROGRAM

Incidences of structural degradation related to the concrete components in NPPs indicate a potential need for improved surveillance, inspection/testing, and maintenance to enhance the technical bases for assuring continued safe operation. The Structural Aging (SAG) Program was initiated in 1988 and had the overall objective of preparing documentation that provides USNRC license reviewers with (1) identification and evaluation of the potential structural degradation processes; (2) issues to be addressed under NPP continued service reviews, as well as criteria, and their bases, for resolution of these issues; (3) identification and evaluation of relevant in-service inspection, structural assessment or remedial measures programs; and (4) methodologies to perform current assessments and reliability-based life predictions of safety-related concrete structures. To meet this objective, SAG Program activities were conducted under three task areas: (1) materials property database, (2) structural component assessment/repair technologies, and (3) quantitative methodology for continued service determinations.

3.1 Material Properties Database

The objective of the materials property database task was to develop a reference source that contains data and information on the time variation of material properties under the influence of pertinent environmental stressors and aging factors. This source can be used to assist in the prediction of potential long-term deterioration of critical structural components in NPPs and to establish limits on hostile environmental exposure for these structures. Primary activities under this task included development of the Structural Materials Information Center, assemblage of materials property data, and review and evaluation of service life models. In addition, durability assessments of concrete structures contained in several nuclear power stations located in the United Kingdom (UK) were conducted, and the performance of post-tensioning systems in both UK and U.S. nuclear power facilities was assessed.

3.1.1 Structural Materials Information Center

Initial development of the *Structural Materials Information Center* (SMIC) has been completed.¹⁴ Contained in this reference are detailed descriptions of the *Structural Materials Handbook* and *Structural Materials Electronic Data Base* that comprise the SMIC. The *Structural Materials Handbook* is an expandable, hard-copy reference document containing complete sets of data and information for each material. The *Structural Materials Electronic Data Base* is an electronically-accessible version of the handbook that provides an efficient means for searching the data base files.

3.1.2 Data Collection

In parallel with efforts to develop the SMIC, activities were conducted to establish materials property data for input into the SMIC. Two primary approaches were utilized — open-literature information and testing of prototypical samples. To date, 144 material data bases have been developed based on open-literature sources (i.e., 128 concrete, 12 metallic reinforcement, 1 prestressing tendon, 2 structural steel, and 1 rubber). Several of these data

bases relate to testing of prototypical samples associated with NPP facilities. Reference 15 provides summary descriptions of the material property database files contained in SMIC.

3.1.3 Material Behavior Modeling

A review and evaluation of accelerated aging techniques and tests that can either provide data for service life models or that by themselves can be used to predict the service life or performance of reinforced concrete was completed.¹⁶ The most promising approach for predicting the remaining service life of concrete utilizes models of the degradation processes. Models were identified and evaluated for each of the degradation processes that can potentially impact the performance of NPP concrete structures. A major conclusion was that theoretical models need to be developed, rather than relying solely on empirical models, because predictions from theoretical models are more reliable, require far less data, and have wider applications.

Durability assessments of concrete structures at UK nuclear power stations, and performance evaluations of post-tensioning systems in UK prestressed concrete reactor vessels (PCRVs) and U.S. post-tensioned containments were conducted.¹⁷⁻¹⁹ Results indicate that the performance of the concrete structures and PCRVs at UK nuclear power stations has been good with only minor incidences of concrete cracking and tendon corrosion (insignificant pits) reported. Performance of the post-tensioning systems in the U.S. containments has been good.

3.2 Structural Component Assessment/Repair Technologies

New structures can be designed for improved durability based on operating experience (e.g., use of high performance concrete materials). Existing structures, however, have already been designed and constructed, so apart from possibly the addition of barrier materials and sealants to accessible surfaces to prevent ingress of hostile environments, the most prudent approach to maintaining adequate structural margins is through an aging management program that involves application of in-service inspection and maintenance strategies.

The objectives of this task were to develop a systematic methodology that can be used to make quantitative assessments of the presence, magnitude, and significance of any environmental stressors or aging factors that can adversely impact the durability of safety-related concrete structures in NPPs; and provide recommended in-service inspection or sampling procedures that can be utilized to develop the data required both for evaluating the current condition of concrete structures and for trending the performance of these components. Associated activities included an assessment of techniques for repair of concrete, and identification and evaluation of techniques for mitigation of any environmental stressors or aging factors that may act on critical concrete components.

3.2.1 LWR Critical Concrete Component Classification

A methodology was developed that provides a logical basis for identifying the critical concrete structural elements in a NPP and the degradation factors that can potentially impact their performance.²⁰ Numerical ranking systems were established to indicate the relative importance of a structure's subelements, the safety significance of each structure, and the potential influence of the particular environment to which it is exposed. Results of this activity can be utilized as part of an aging management program to prioritize in-service inspection activities.

An evaluation of the impact on plant risk due to structural aging can also be used in the selection of structural components for evaluation. The increase in risk due to structural aging can be examined within the framework of a seismic probabilistic risk assessment²¹ and involves four primary steps: (1) identify a seismic hazard from potential seismogenic sources, historical seismicity in the vicinity of the plant, and attenuation of ground motion to the site; (2) develop a plant logic model to explain the interaction of various plant components and systems in mitigating the effects of initiating events; (3) develop fragility models to determine capacity of plant components and systems probabilistically; and (4) measure risk by calculating core damage probability, plant damage state, and off-site consequences to public health and safety. Plant logic models are used to identify structural components of most importance and the impact of aging on the fragility parameters is evaluated through changes in the cumulative distribution function of the estimated probability of core damage; the high-confidence, low-probability-of-failure

acceleration; and a point estimate of risk. Reference 22 indicates that substantial damage to structural components due to aging may lead to less than an order of magnitude increase in core damage probability.

The recommended strategy for use in component selection for evaluation is to use a combination of the above approaches. In this manner, in-service condition assessments (or periodic maintenance actions) that may be required for continued service evaluations can focus on a selected subset of structural components that have the potential to impact plant safety.

3.2.2 NDE Sampling/Inspection Technology

Direct and indirect techniques used to detect degradation of reinforced concrete structures were reviewed.²³ Capabilities, accuracies, and limitations of candidate techniques were established (e.g., audio, electrical, infrared thermography, magnetic, stress wave reflection/refraction, radioactive/nuclear, rebound hammer, and ultrasonic). Information was assembled on destructive (e.g., coring, probe penetration, and pull-out) and emerging (e.g., leakage flux, nuclear magnetic resonance, and capacitance-based) techniques. Recommendations were developed on application of testing methods to identify and assess damage resulting from typical factors that can degrade reinforced concrete. Also, statistical data were established for nondestructive testing techniques commonly used to indicate concrete compressive strength (i.e., break-off, pull-out, rebound hammer, ultrasonic pulse velocity, and probe penetration).²⁴ This information is required where destructive and nondestructive tests cannot be conducted in tandem at noncritical locations to develop a regression relation between the parameter measured and the concrete compressive strength. The methods developed can be used to estimate variance in strength or to yield information about distribution of strength population that is required to calculate the characteristic strength for use in structural integrity assessments.

3.2.3 Remedial/Preventative Measures Considerations

Corrosion resulting from either carbonation or the presence of chlorides is the dominant type of distress that impacts reinforced concrete structures. Corrosion mechanisms and types (e.g., uniform, pitting, bimetallic, crevice, etc.) as well as conditions that affect the corrosion rate (e.g., oxygen, electrolyte conductivity, ion concentration, temperature, etc.) were identified.²⁵ Methods available to detect corrosion occurrence include visual observations, half-cell potential measurements, delamination detection, electrolyte chemistry, corrosion monitors, acoustic emission, radiography, ultrasonics, magnetic perturbation, metallurgical evaluations, and electrical resistance. Remedial measures include damage repair, cathodic protection, inhibitors, chloride removal, membrane sealers, stray current shielding, dielectric isolation, coatings, and environmental modifications. Stray electrical current resulting from any of a number of sources (e.g., cathodic protection systems, high voltage direct current systems, and welding operations) could also lead to corrosion. Techniques to detect stray current include half-cell potential versus time measurements, half-cell potential versus distance measurements, and cooperative (interference) testing. Mitigation measures for stray current include prevention or elimination of the current source, installation of cathodic protection, draining the current from the source, and shielding the structure from the source. Use of sacrificial or impressed current cathodic protection systems as both a rehabilitation technique for corroding structures and a corrosion prevention technique for steel that may lose its inherent passivity at a later date were investigated. Design considerations, advantages and disadvantages, and commentary on when cathodic protection should and should not be used were also addressed.

Damage repair practices commonly used for reinforced concrete structures in Europe and North America were reviewed.^{26,27} In Europe, activities have concentrated on repair of damage resulting from corrosion of steel reinforcement. Basic repair solutions include: (1) realkalization by either direct replacement of contaminated concrete with new concrete, use of a cementitious material overlay, or application of electrochemical means to accelerate diffusion of alkalis into carbonated concrete; (2) limiting the corrosion rate by changing the environment (e.g., drying) to reduce the electrolytic conductivity; (3) steel reinforcement coating (e.g., epoxy); (4) chloride extraction by passing an electric current (DC) from an anode attached to the concrete surface through the concrete to the reinforcement (chloride ions migrate to anode); and (5) cathodic protection. Repair strategies and procedures were

developed in the form of flow diagrams. Information specifically addressing inspection, degradation, and repair of concrete structures in LWR plants was assembled through a questionnaire sent to U.S. utilities.²⁷ Responses provided by 29 sites representing 41 units indicate that the majority of the plants perform inspections of concrete structures only in compliance with integrated-leakage-rate test requirements (visual inspections), and surveillances of the post-tensioning systems of prestressed concrete containments. The most common deterioration causes were drying shrinkage, acid/chemical attack, thermal movement, freeze-thaw cycles, and seawater exposure. Most of the repair activities were associated with problems during initial construction (cracks, spalls, and delaminations), with the repairs performed on an as-needed basis. When the performance of a repair was evaluated, visual inspection was used. Detailed information on repair materials and techniques is presented in Ref. 27.

3.2.4 Considerations for Development of an In-Service Inspection Program

Assessing the ability of NPP concrete components to continue to meet their functional and performance requirements is an essential part of an aging management program. Given the complex nature of the various environmental stressors and aging factors that potentially can exert deteriorating influences on the concrete components, a systems approach is probably best. Such an approach would encompass the development of: (1) a classification scheme for structures and elements, and deterioration causes and effects; (2) a methodology for conducting a quantitative assessment of the presence of active deteriorating influences; and (3) remedial measure considerations to reestablish the capability of degraded structures or components to meet potential future requirements, such as a loss-of-coolant accident (LOCA). As items (1) and (3) have been addressed previously in this section, only pertinent aspects of item (2) will be addressed (i.e., condition assessments, inspection methods, acceptance criteria, and scheduling). An example illustrating development and application of an inspection program to a PWR with large-dry post-tensioned concrete containment is provided in Ref. 28.

3.2.4.1 Condition Assessments

Determining the existing performance characteristics and extent and causes of any observed distress is accomplished through a condition assessment. Common in the condition assessment approaches is the conduct of a field survey, involving visual examination and application of nondestructive and destructive testing techniques, followed by laboratory and office studies. References 29 and 30 provide guidelines and direction on conducting surveys of existing buildings.

The condition survey usually begins with a review of the "as-built" drawings and other information pertaining to the original design and construction so that information, such as accessibility and the position and orientation of embedded steel reinforcing and plates in the concrete, is known prior to the site visit. Next is a detailed visual examination of the structure to document easily obtained information on instances that can result from or lead to structural distress such as cracking, spalling, leakage, and construction defects such as honeycombing and cold joints in the concrete. A crack survey is usually done by drawing the locations and widths of cracks on copies of project plans. Cracking patterns may appear that suggest weaknesses in the original design, construction deficiencies, unanticipated thermal movements, chemical reactivity, detrimental environmental exposure, restrained drying shrinkage, or overloading. Distress associated with cracks such as efflorescence, rust stains, or spalling are noted. Photographs or video recordings are made to provide a permanent record of this information, and notes are made on the survey sheets to indicate the area photographed. After the visual survey has been completed, the need for additional surveys such as delamination plane, corrosion, or pachometer is determined. The delamination plane survey is used to identify internal delaminations that are usually caused by corrosion of embedded metals or internal vapor pressure. Results of the visual and delamination surveys are used to select portions of the structure that will be studied in greater detail. To locate areas of corrosion activity within reinforced concrete, copper-copper sulfate half-cell studies can be performed. By taking readings at multiple locations on the concrete surface, an evaluation of the probability of corrosion activity of embedded reinforcing steel (or other metals) can be made. Where significant chloride penetration is suspected, concrete powder samples or cores should be removed from several depths extending to and beyond the embedded outer layer of reinforcing steel. If the concrete contains more acid soluble chloride than

about 300 ppm by weight (0.7 kg/m^3), it is considered to contain sufficient chloride to support electrochemical corrosion of embedded steel when in a moist environment that has oxygen availability. Also, a pachometer survey may be performed as part of the detailed study to confirm the location of steel reinforcement. Where there is evidence of severe corrosion, the steel bar should be uncovered to allow visual inspection and measurement of cross-sectional area loss. Upon return to the office, results of the field survey are evaluated in detail. A crack survey map is prepared and studied for meaningful patterns. Half-cell data are studied and isopotential lines are drawn to assist in determining active corrosion sites. Samples of concrete and steel obtained from areas exhibiting distress are tested in the laboratory. Investigation techniques that may be used include (1) petrography (thickness, distribution of cement, aggregate studies, estimation of water-cementitious materials ratios, air-void distribution, types of distress, recognition of unstable aggregates, deterioration mechanisms, and age at which cracking occurred), (2) chemical (chemical constituents of the cementitious materials, characteristics of the paste and aggregates, presence and quantity of chemical admixtures, quantification of chemical compounds within the cement paste, efflorescence, and carbonation effects), (3) concrete strength testing (compressive strength, modulus of elasticity, tensile strength, flexural strength, and bond strength of patches or overlays), and (4) steel properties (yield and ultimate strengths of reinforcing bars). Chloride ion results are plotted vs depth to determine the profile and the chloride content at the level of the steel. Any elements that appear to be structurally marginal, due either to unconservative design or effects of deterioration, are identified and appropriate calculation checks made. These analyses may identify distress in the structure that has been caused by structural overload and indicate safety factors. If the calculations are inconclusive, suitable load testing may be indicated. After all of the field and laboratory results have been collated and studied and all calculations have been completed, a report is prepared.

3.2.4.2 Inspection Methods

Inspection and testing methods generally fall into three categories: (1) visual inspection, (2) nondestructive and destructive testing, and (3) analytical assessments.

Visual Inspections Visual inspections are one of the most valuable of the condition survey methods because many of the manifestations of concrete deterioration appear as visible indications or discontinuities on exposed concrete surfaces. Visual inspections encompass a variety of techniques (e.g., direct and indirect inspection of exposed surfaces, crack and discontinuity mapping, physical dimensioning, environmental surveying, and protective coatings review). To be most effective, the visual inspection should include all exposed surfaces of the structure; joints and joint materials; interfacing structures and materials (e.g., abutting soil); embedments; and attached components (e.g., base plates and anchor bolts). Degraded areas of significance are measured. The condition of the surrounding structures should also be examined to detect occurrence of differential settlement or note aggressiveness of the local operating environment. Results obtained should be documented and photographs or video images taken of any discontinuities and pertinent findings.

Nondestructive and Destructive Testing Nondestructive testing techniques employ specialized equipment to obtain specific data about the structure in question, and in certain instances (e.g., inaccessible surfaces) its surrounding environment (i.e., structure-specific or environment-specific). The structure-specific methods are used to inspect internal portions of the structure for discontinuities (e.g., presence of voids, cracks, and steel reinforcement) or to provide an indication of constituent material characteristics (e.g., compressive strength, modulus of elasticity, and size and location of steel reinforcement). Generally, the most comprehensive means of assessing structural condition and increasing the probability of defect detection is to use two or more of these techniques in tandem (e.g., ultrasonic pulse velocity and rebound hammer). Environment-specific methods are used where surfaces of structures are not accessible for direct inspection due to the presence of soils, protective coatings, or portions of adjacent structures. These methods are used to provide an indirect assessment of the physical condition of the structure (i.e., potential for degradation) by qualifying the aggressiveness of the environment adjacent to the structure (e.g., air, soil, and groundwater). If results of these tests indicate that the environment adjacent to the structure is not aggressive, one might conclude that the structure is not deteriorating. However, when conditions indicate that the environment is potentially conducive to degradation, additional assessments are required that may include exposure of the structure for visual or limited destructive testing.

Destructive testing involves the removal of samples of material from the structure for the purpose of determining physical, chemical, or mechanical characteristics. Since destructive testing involves a direct examination of the material sample removed, it provides information of significant value for use in aging management programs. Both the presence and impact of deterioration can be determined quantitatively. Also, supplemental testing can be done using these samples to indicate future performance (e.g., durability evaluations through accelerated testing techniques, and to determine the potential for alkali-aggregate reactions). Reference 31 presents a summary listing of evaluation procedures routinely used to determine concrete properties, the physical condition of concrete, and the occurrence or potential for occurrence of corrosion of steel either embedded in or in contact with concrete.

Analytical Assessments Analytical methods involve the use of supplemental calculations or analytical procedures to re-evaluate the behavior and resistance of the structure (e.g., structural margins determinations). This re-evaluation may be required due to either a change in performance requirements (e.g., plant modification) or the identification of deterioration. Finite-element and ultimate strength design methods provide two techniques for reanalysis.

3.2.4.3 Acceptance Criteria

Material discontinuities, such as steel impurities or local regions of improper concrete consolidation, unless excessive, are generally of minor structural significance. However, errors during construction and the initiation and propagation of various degradation mechanisms may result in loss of function and load-carrying ability. Degradation mechanisms often occur at time-varying rates (e.g., chemical attack or migration of chloride ions). In-service inspections of structures at risk are conducted to identify and mitigate the potential degradation factor effects before a repair is required or structural margins have eroded to unacceptable levels.

Cracking is a very common damage by-product from a large number of concrete degradation mechanisms. Active concrete cracking is difficult to assess in terms of impact on structural behavior and is difficult to repair. Thus, inspection methods that support the early identification, sizing, and cause of cracking in concrete structures are of primary interest for future inspections. Also, the primary concern for all metallic constituents of concrete structures is corrosion and corrosion-related damage. Inspections that identify early signs of corrosion cell initiation and indicate the rate of propagation are similarly valuable.

Two approaches have been developed for assistance in the classification and treatment of conditions or findings that might emanate from in-service inspections of NPP reinforced concrete structures. These approaches are based primarily on the results of visual inspections since these inspections provide the cornerstone of any condition assessment program for concrete structures. Also, with the exception of some guidance on half-cell potential and ultrasonic pulse velocity measurements, few standards have been published presenting acceptance criteria for results obtained from nondestructive evaluation tests. The information below is provided only as a basis for development of acceptance criteria as each structure is unique due to its application, geometry, materials of construction and environmental exposure.

Visual-Based Approach The visual-based approach uses a "three-tiered" hierarchy.^{28,32} Through use of different levels of acceptance, minor discontinuities can be accepted and more significant degradation in the form of defects can be evaluated in more detail. The three acceptance levels include acceptance without further evaluation, acceptance after review, and additional evaluation required.

Conditions presented below are considered not to require further evaluation. Reference 33 provides definitions and pictorial representations of typical forms of concrete degradation. In the event that the conditions provided below are exceeded, or observed conditions are determined to be deserving further evaluation, a more detailed review is required. Structures that are partially or totally inaccessible for visual inspections may require supplemental evaluations as environments may be present that are conducive to degradation.

1. Unlined Concrete Surfaces – Concrete surfaces that are exposed for inspection and meet the following surface condition attributes are generally acceptable without further evaluation if the following criteria are met:
 - a. Absence of leaching and chemical attack;

- b. Absence of abrasion, erosion, and cavitation;
 - c. Absence of drummy areas (poorly consolidated with paste deficiencies);
 - d. Popouts and voids less than 20 mm in diameter or equivalent surface area;
 - e. Scaling less than 5 mm in depth;
 - f. Spalling less than 10 mm in depth and 100 mm in any dimension;
 - g. Absence of any signs of corrosion in reinforcing steel system or anchorage components (including concrete staining or spalling);
 - h. Passive cracks less than 0.4 mm in maximum width ("passive cracks" are defined as those having an absence of recent growth and absence of other degradation mechanisms such as leaching at the crack);
 - i. Absence of excessive deflections, differential settlements, or other physical movements that may affect structural performance; and
 - j. Absence of cement-aggregate reactions, chemical attack, fire damage, or other active degradation mechanism.
2. Concrete Surfaces Lined by Metal or Plastic – Concrete structures with inner surfaces protectively lined with either a metallic or plastic (non-metallic) system are judged to be acceptable without further evaluation if the following criteria are met:
- a. Without Active Leak Detection System
 - 1. Absence of bulges or depressions in liner plate (those that appear age-related as opposed to being created during construction);
 - 2. Absence of corrosion or other liner damage; and
 - 3. Absence of cracking in liner weld or base metal.
 - b. With Active Leak Detection System
 - 1. No detectable leakage observed in leak detection system;
 - 2. Absence of any liner damage, such as noted in 2(a) above; and
 - 3. Absence of fluid penetration indications by other detection systems.
3. Areas Around Embedments in Concrete – The condition of the concrete around embedments is acceptable without further evaluation if the following criteria are met:
- a. Concrete surface condition attributes of Criteria 1 above are met;
 - b. Absence of corrosion on the exposed surfaces of embedded metal members and corrosion staining around the embedded metal;
 - c. Absence of detached embedments or loose anchorages; and
 - d. Absence of degradation due to vibratory loads from piping and other attached equipment.
4. Joints, Coatings, and Non-Structural Components – The condition of joints, protective coatings, waterproofing membranes, and other non-structural elements is acceptable without further evaluation if the following criteria are met:*
- a. No signs of separation, environmental degradation, or water in-leakage are present in coatings, joints, or joint sealant material;
 - b. Loss or degraded areas of coatings for structures that do not serve as a barrier to aggressive chemical flows are limited in surface area to 4000 square millimeters or less at one area, and 0.01 square meters over the gross surfaces of the structure;
 - c. Absence of degradation in any waterproofing membrane protecting below-grade concrete surfaces (within the inspected area); and
 - d. Non-structural components such as dewatering systems are serving their intended function.
5. Post-Tensioning Systems – Components of post-tensioning systems are acceptable if requirements such as provided in Ref. 34 are met.

Findings listed below require review and interpretation in order to evaluate acceptability. Such a review involves determining the likely source of degradation, its activity level, and its net effect on the component. Based on results of the review and evaluation, possible approaches include acceptance as-is, further evaluation using

* Information on protective coatings for NPP applications is provided in Refs. 35 and 36.

enhanced visual inspection (e.g., magnification), scheduling follow-up inspections at a later date, or use of nondestructive or destructive testing techniques. An analytical assessment of the necessity for repair may also be required. The analytical assessment should examine the impact of existing degradation on the performance characteristics of the structure. Accessibility of the components in question will also enter into the decision process relative to the action to be taken.

1. Unlined Concrete Surfaces – The following surface conditions shall be reviewed to determine if they are either acceptable, require further evaluation, or require repair. Discontinuities exceeding the quantitative limits below require additional evaluation.
 - a. Appearance of leaching or chemical attack;
 - b. Areas of abrasion, erosion, and cavitation degradation;
 - c. Drummy areas that may exceed the cover concrete thickness in depth;
 - d. Popouts and voids greater than 20 mm but less than 50 mm in diameter or equivalent surface area;
 - e. Scaling greater than 5 mm but less than 20 mm in depth;
 - f. Spalling greater than 10 mm but less than 20 mm in depth, and less than 200 mm in any planar dimension;
 - g. Corrosion staining on concrete surfaces;
 - h. Passive cracks greater than 0.4 mm but less than 1 mm in maximum width; and
 - i. Passive settlements or deflections exceeding the original design limits or expected value.
2. Concrete Surfaces Lined by Metal or Plastic
 - a. Without Active Leak Detection System – Presence of any condition listed in Criteria 2(a) of previous section shall be further evaluated to determine acceptability; and
 - b. With Active Leak Detection System – Presence of leakage in excess of amounts and flow rates committed to in the original design or Plant Technical Specification will necessitate a root cause investigation and assessment of the need for follow-up action. Leakage within the prescribed limits may be acceptable if the source is known and found to be inconsequential.
3. Areas Around Embedments in Concrete – Presence of any condition listed in Criteria 1 above for concrete surfaces or presence of any of the attributes presented in Criteria 3(b) through 3(d) of previous section shall be further evaluated to determine acceptability.
4. Joints, Coatings, and Non-Structural Components – Presence of any condition exceeding the descriptions and limits of Criteria 4 in previous section shall be further evaluated to determine acceptability. Any observation of widespread adhesion/cohesion problems, environmental attack, or poor performance indicators are considered unacceptable.

Conditions outside the criteria provided in the previous two sections must be evaluated to determine the appropriate course of action. This will generally involve extensive application of both nondestructive and destructive testing methods. Detailed analytical evaluations frequently will be required to better characterize the current condition of the structure and provide the basis for formulation of a repair strategy (if needed). Even if the analysis results indicate that the component is acceptable at present, additional assessments should be conducted to demonstrate that the component will continue to meet its functional and performance requirements during the desired service life (i.e., take into account the current structural condition and use service life models to estimate the future impact of pertinent degradation factors on performance). If the structure's desired service life is short, and its loss of function due to degradation is occurring at a rate such that sufficient structural margins will be maintained during this period, no action may be required. However, when the opposite is true and loss of function due to degradation is occurring at a rate such that structural margins will not be adequately maintained during the desired service life period, analytical and test results should be utilized to develop an in-service inspection/repair strategy that will maintain structural margins during the desired service life.

Degradation-Based Approach The effects of degradation mechanisms on the performance of a structure can range from cosmetic to structurally degrading. Provided below is information intended to be of assistance in quantifying the significance of degradation that is detected through visual inspections, nondestructive testing, or a combination of these methods. General criteria are developed in terms of parameters associated with corrosion of

steel embedded in concrete that can be measured. Guidance for detection and resolution of degradation in the form of corrosion of post-tensioning system components and loss of prestressing force are not addressed as detailed information on these topics is available in Ref. 34.

Cracking in concrete can result from a number of factors as noted previously. Designs of reinforced concrete structures generally consider that the concrete is incapable of supporting tensile forces. Steel reinforcement is included in the structural members to both carry the tensile loadings and to provide control of cracking (i.e., limit width and spacing of concrete cracks). Both the width of concrete cracks and the environmental exposure are important. From an aging management perspective, the presence of concrete cracks is of importance because they provide possible avenues of access for environmental stressors (e.g., chloride ions and sulfate solutions). As noted in Ref. 27, there also have been a number of studies over the years that relate maximum permissible concrete crack widths to environmental factors. Some work has been done in classifying environmental exposure conditions in terms of their degree of aggressivity. For example, Ref. 37 provides information on the influence of the moisture condition on several durability processes (e.g., carbonation, corrosion, frost attack, and chemical attack). Also, exposure classes have been developed that relate specific environmental conditions to steel reinforcement corrosion, and degree of chemical attack of concrete by soils and water containing aggressive agents.^{37,38}

Two damage-state charts have been prepared to assist in the resolution of results obtained from in-service inspections or testing. Figure 1 provides a relationship between environmental exposure in terms of extent of carbonation or chloride ion content of the environment, the width of cracks present, and the necessity for additional evaluation or repair. As noted in the figure, the extent of action required increases as the severity of environmental

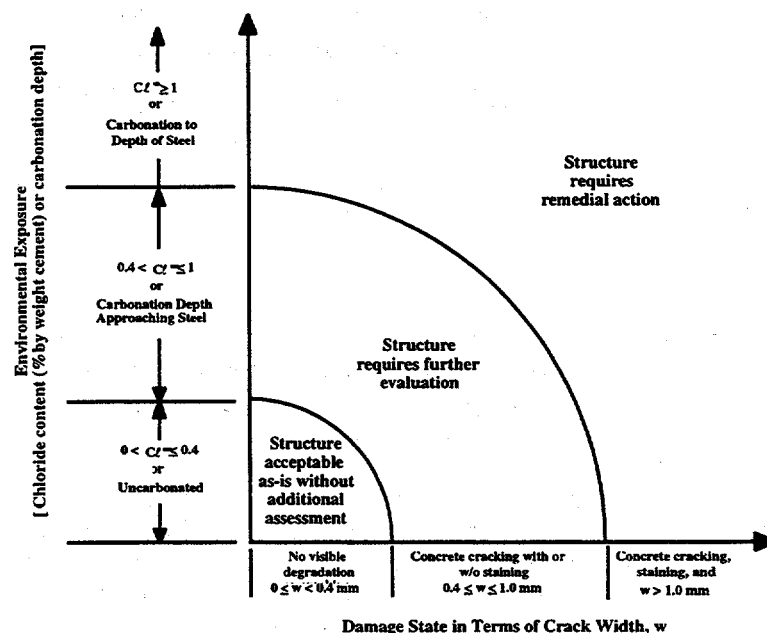


Fig. 1 Damage state chart relating environmental exposure, crack width, and necessity for additional evaluation or repair.

exposure increases or the width of cracks present increases. Figure 2 provides a relationship between environmental exposure, half-cell potential readings, and necessity for further evaluation or repair. Superimposed on the half-cell potential axis are visual inspection results that might be anticipated for different degrees of severity of corrosion of steel reinforcement. Crack width information presented in Ref. 27 were used to develop the relationships presented in these figures. Further evaluation would consider the use of other inspection, testing, or analytical tools to obtain additional information on the current condition of the structure and the potential for further degradation with time.

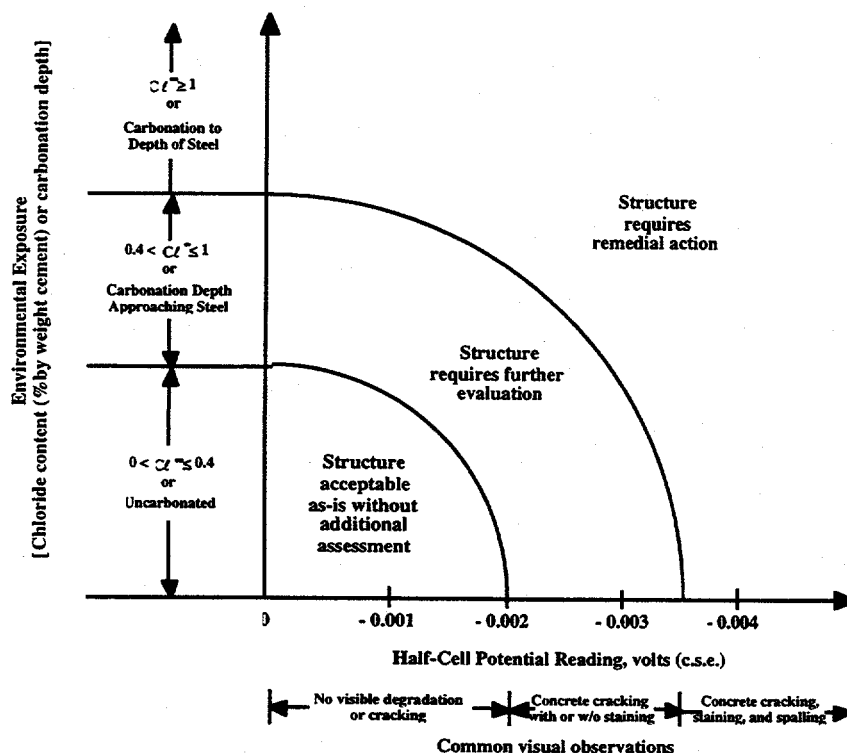


Fig. 2 Damage state chart relating environmental exposure, half-cell potential reading, and necessity for additional evaluation or repair.

3.2.4.4 Inspection Scheduling

In-service inspection programs for NPPs have traditionally focused on the concrete containment vessel. Schedules for the containment-related inspections are considered to be "outage based" in that most or all of these inspections are performed during planned plant outages (e.g., refueling) to provide improved plant access. The plant owner does have the option of electing to perform certain inspections (e.g., tendon surveillances) at other times as long as the code-mandated frequencies and schedules are met.

References 28 and 32 provide information on recommended frequencies and schedules for conducting inspections of safety-related concrete structures other than containments. These schedules take into account the relative aggressiveness of environmental conditions and physical exposures of these structures, and help assure that any age-related degradation is detected at an early stage of development so appropriate mitigative actions can be taken. Ten-year inspection intervals are recommended for structures located below-grade or in a controlled interior environment. Five-year inspection intervals are recommended for other structures. These frequencies may be modified to smaller intervals if plant environments are particularly severe or degradation has been observed to occur. When the observed degradation exceeds criteria provided previously, increased visual inspections should be supplemented by nondestructive, and possibly destructive testing. Reliability-based methods also can be used to schedule inspections of safety-related concrete structures. These methods assess the reliability of the NPP reinforced concrete structures in terms of damage state and rate of degradation, inspection method detectability functions, remedial actions, and frequency of inspection. Optimized strategies for inspection and maintenance can be developed that minimize future costs associated with inspection, repair, and loss of service, while maintaining the component probability of failure at or below a target value over the service life of the structures.³⁹⁻⁴¹

3.3 Quantitative Methodology for Continued Service Determinations

This task's goal was to develop a methodology to facilitate quantitative assessments of current and future structural reliability and performance of concrete structures in nuclear plants. Specific objectives associated with accomplishing this goal include (1) identification of models to evaluate changes in strength of concrete structures over time and (2) formulation of a methodology to predict structural reliability of existing concrete structures during future operating periods.

3.3.1 Time-Dependent Reliability Analysis

Structural loads, variations in engineering material properties, and strength degradation mechanisms are random in nature. Time-dependent reliability analysis methods provide a framework for performing condition assessments of existing structures and for determining whether in-service inspection and maintenance are required to maintain reliability and performance at the desired regulatory level.

The strength, $R(t)$, of the component and the applied loads, $S(t)$, both are functions of time. At any time, t , the margin of safety, $M(t)$, is

$$M(t) = R(t) - S(t). \quad (1)$$

Making the customary assumption that R and S are statistically independent random variables, the (instantaneous) probability of failure is,

$$P_f(t) = P[M(t) < 0] = \int_0^\infty F_R(x) f_S(x) dx. \quad (2)$$

in which $F_R(x)$ and $f_S(x)$ are the probability distribution function of R and density function of S . Equation 2 provides one quantitative measure of structural reliability and performance, provided that P_f can be estimated and validated.

For service life prediction and reliability assessment, one is more interested in the probability of satisfactory performance over some period of time, say $(0,t)$, rather than the snapshot of the reliability of the structure at a particular time provided by Eqn. 2. Indeed, it is difficult to use reliability analysis for engineering decisions without having some time period (say, an in-service inspection or maintenance interval) in mind. The probability that a structure survives during interval of time $(0,t)$ is defined by a reliability function, $L(0,t)$. If, for example, n discrete loads $S_1, S_2, \dots, S_n$ occur at times $t_1, t_2, \dots, t_n$ during $(0,t)$, the reliability function becomes,

$$L(0, t) = P[R(t_1) > S_1, \dots, R(t_n) > S_n]. \quad (3)$$

If the load process is continuous rather than discrete, there is an analogous but more complex expression.

The conditional probability of failure within time interval $(t, t+dt)$, given that the component has survived during $(0,t)$, is defined by the hazard function:

$$h(t) = -d(\ln L(0,t))/dt. \quad (4)$$

Solving for $L(0,t)$ yields,

$$L(0, t) = \exp \left[-\int_0^t h(x) dx \right]. \quad (5)$$

The hazard function is especially useful in analyzing structural failures due to aging or deterioration. For example, the probability that time to structural failure, T_f , occurs prior to a future maintenance operation scheduled at $t + \Delta t$, given that the structure has survived to t , can be evaluated as,

$$P[T_f \leq t + \Delta t | T_f > t] = 1 - \exp \left[- \int_t^{t + \Delta t} h(x) dx \right]. \quad (6)$$

The hazard function for pure chance failures (case 1 in next section) is constant. When structural aging occurs and strength deteriorates, $h(t)$ characteristically increases with time. In-service inspection and maintenance impact the hazard function, causing it to change discontinuously at the time an inspection is performed. The main difference between time-dependent reliability of undegrading and degrading structural components can be characterized by their hazard functions.

Significant structural loads often can be modeled as a sequence of load pulses, the occurrence of which is described by a Poisson process with mean rate of occurrence λ , duration τ , and random intensity S_j (see Fig. 3). Such a simple load process has been shown to be an effective model for extreme loads on structures, since normal service loads challenge the structure to only a small fraction of its strength. With this assumption, the reliability function becomes³⁹

$$L(0, t) = \int_0^\infty \exp \left(-\lambda t \left[1 - t^{-1} \int_0^t F_S(r g) dt \right] \right) f_R(r) dr, \quad (7)$$

in which $f_R(r)$ is the probability density function of initial strength, $R(0)$, and $g(t)$ equals the mean of $R(t)/R(0)$, a function describing the degradation of strength in time (see Fig. 3). The limit state probability, or probability of failure during $(0, t)$, can be determined as $F(t) = 1 - L(0, t)$; it should be noted that $F(t)$ is not the same as $P_f(t)$ in Eqn. 2.

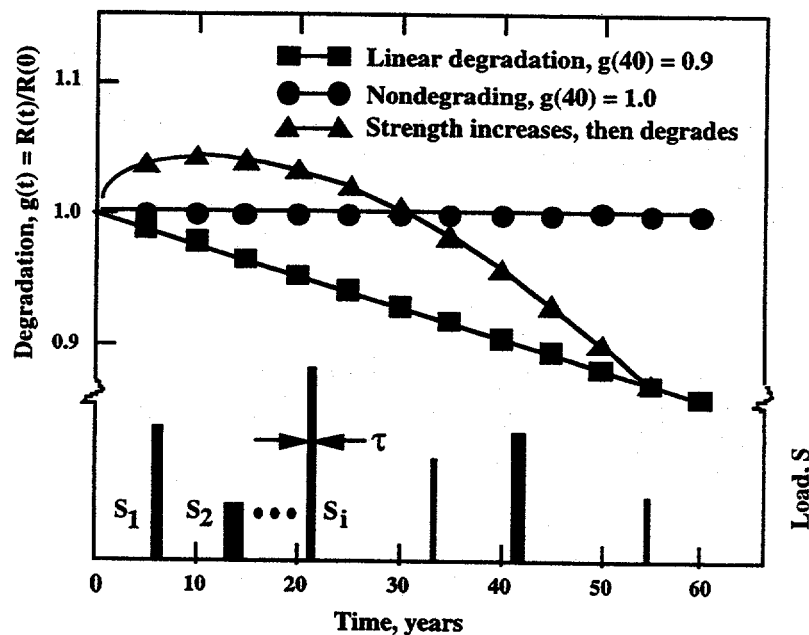


Fig. 3 Mean degradation functions of one-way slab.

3.4.2 Service Life Predictions for Reinforced Concrete Slab

Time-dependent reliability concepts are illustrated with a simple example of a concrete slab drawn from Refs. 39 and 40 that was designed using the requirements for flexure strength found in ACI Standards (Refs. 8 and 42):

$$0.9 R_n = 1.4 D_n + 1.7 L_n, \quad (8)$$

in which R_n is the nominal or code resistance, and D_n and L_n are the code-specified dead and live loads, respectively. The strength of the slab changes in time, initially increasing as the concrete matures and then decreasing due to (unspecified) environmental attack. This situation is illustrated conceptually by the sample functions $r(t)$ and $s(t)$ for strength and load in Fig. 3. The behavior of the resistance over time must be obtained from mathematical models describing the degradation mechanism(s) present.

Figure 4 presents a comparison of limit state probabilities for intervals $(0, t)$ for t ranging up to 60 years. Three cases are presented (see Fig. 3): (1) no degradation in strength, i.e., $R(t) = R(0)$, a random variable; (2) $R(t)$ initially increasing with concrete maturity and then decreasing; and (3) $R(t)$ decreasing linearly over time to 90% of its initial strength at 40 years. The statistics used in the illustrations that follow are summarized in Table 1 (Ref. 43). Neglecting strength degradation entirely in a time-dependent reliability assessment can be quite unconservative, depending on the nature of the time-dependent behavior.

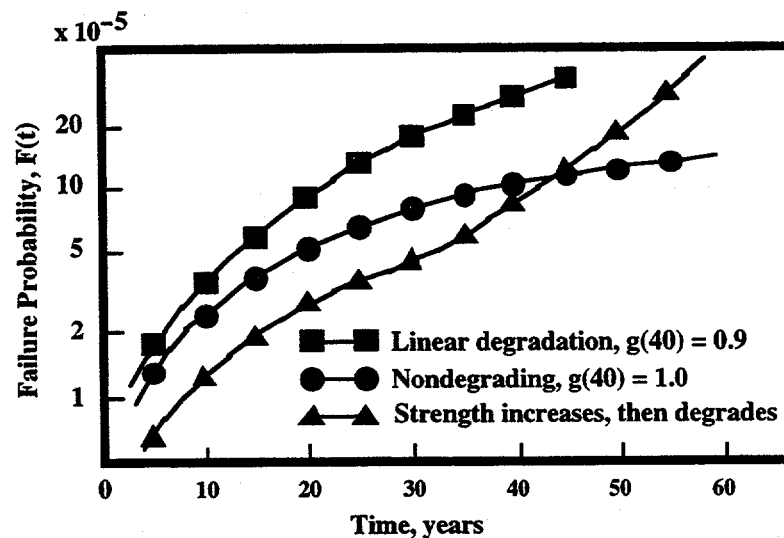


Fig. 4 Failure probability of one-way slab.

3.4.3 Service Life Predictions for Reinforced Concrete Shear Wall

In the probability-based method described above to estimate the strength degradation of a component and to evaluate the effect of periodic maintenance from a reliability point of view^{39,40} it was assumed that strength degradation at any section is caused by one randomly occurring defect of random intensity. Such a model is reasonable when the degradation is such that at most one defect or zone of damage is likely to occur within a given cross section. However, there are cases where several defects or zones of damage may contribute in reducing strength. The evaluation of the (random) residual strength of the wall requires that the cumulative effect of defects in a cross section be considered. Recent research has provided a method whereby the impact of randomly occurring multiple defects on structural capacity can be considered. Some results are summarized in the following discussions.⁴⁴

Table 1 Statistical properties of strength and load.*

Parameter	Rate of Occurrence	Duration	Main	C.O.V.	Pdf
Flexure Strength	—	—	1.12 M_n	0.14	Lognormal
Shear Strength	—	—	1.7 V_n	0.18	Lognormal
Dead Load	—	—	1.0 D_n	0.07	Normal
Live Load	0.5/yr	3 mo.	0.4 L_n	0.50	Type I
Earthquake Load	0.05/yr	30 sec.	0.08 E_{sse}	0.85	Type II

* The nominal values M_n , V_n , and P_n are the capacities that would be computed from Refs. 8 and 42.

A low-rise shear wall with a height-to-width ratio equal to one is considered. It is subjected to vertical load, D , that is uniformly distributed on the top of the wall, and in-plane lateral load, V , that is concentrated at the top of the wall. The shear strength of concrete walls can be estimated from empirical models provided by Refs. 8 and 45. These models are not sufficient to analyze the strength of deteriorating low-rise shear walls. Although finite-element analysis is versatile and able to provide detailed information on the shear resistance mechanisms, it requires lengthy computational effort, especially when adapted to reliability analysis. A recent theoretical approach for evaluating shear strength of reinforced concrete components determines the ultimate shear strength as the sum of the forces sustained by truss, V_t , and arch, V_a , mechanisms.⁴⁶⁻⁴⁸ It is assumed that the wall fails if all the reinforcing bars yield in tension and the concrete arch crushes in compression. According to the lower bound theorem of plasticity,⁴⁹ this approach provides a conservative estimate of the shear strength. These models were modified for the reliability analysis of a degrading low-rise concrete shear wall.⁴⁴ Figure 5 shows that the strength predicted by this method compares well to experimental tests of low-rise shear walls.^{50,51}

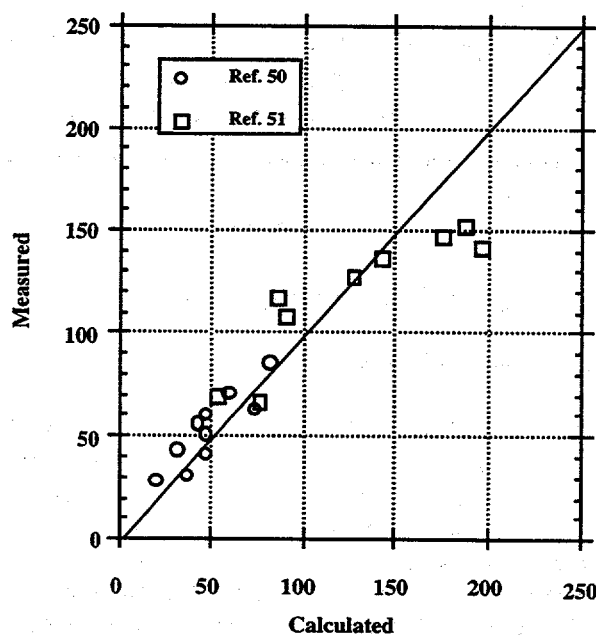


Fig. 5 Comparison of measured and calculated shear strength.

Wall in Shear A wall subjected to expansive aggregate reaction or chemical attack suffers a loss of concrete section. If the wall is not heavily reinforced in the transverse direction, the contribution of the truss mechanism is small. Thus, it can be assumed that only the strength of the arch mechanism decreases due to the loss of concrete section while the strength attributed to the truss mechanism is independent of the degradation. If the wall is reinforced in the longitudinal direction, the vertical reaction is sustained by the longitudinal reinforcement, and degradation of concrete outside the concrete strut in the arch mechanism can be neglected. Assuming that the stress in the concrete strut is uniform, the degradation function of the shear wall can be given by

$$\begin{aligned} \zeta(t) &= \frac{V_t + V_a(t)}{V_{u0}} \\ &= \frac{V_t + G_a(t) V_a(0)}{V_{u0}} \end{aligned} \quad (9)$$

in which V_{u0} is the initial shear strength of the wall, $V_a(t)$ is the shear strength of the arch mechanism at time t , and $G_a(t)$ is the degradation function of the shear strength of the arch mechanism.

Wall in Flexure and Compression. The ultimate flexural capacity of a wall cross section is

$$M_u = T_s \left(\frac{b}{2} - d_c \right) + C_c \left(\frac{b}{2} - k_2 c_u \right) + C_s \left(\frac{b}{2} - d_c \right) \quad (10)$$

in which T_s and C_s are the total force transferred to reinforcement in the tension and compression zone, respectively, d_c is the concrete cover, c_u is the distance from the compressive face to the neutral axis, and $k_2 c_u$ locates the compressive resultant, C_c . For illustration, assume that

- The wall is subjected to time-invariant dead load, D , that is uniformly distributed, and intermittent lateral load V , that is concentrated at the top of the wall and may act either in-plane or out-of-plane.
- The wall is designed for in-plane shear based on the current design requirement of ACI Committee 349 (Ref. 42)

$$0.9R_n = E_{ss} \quad (11)$$

in which R_n is the nominal shear strength and E_{ss} is the structural action due to safe-shutdown earthquake. The statistical characteristics of the shear strength and the earthquake load are shown in Table 1. It is assumed that $E_{ss} = 3D = 3.21$ MN.

- The mean initiation rate of local damage per unit surface area due to expansive aggregate reaction is time invariant and is $0.1/\text{m}^2/\text{year}$.
- The defect intensity is modeled as,

$$Y(t) = C(t - T_I)^2 \quad (12)$$

in which C is a time-invariant random variable described by a lognormal distribution with mean value, m_c , of $2.22 \times 10^{-6}/\text{year}$ and coefficient of variation, V_C , of 0.5. This value results in an average defect size large enough after several years following its initiation to be found by visual inspection.

- The 28-day specified compressive strength of concrete equals 27.6 MPa. According to Ref. 52, the corresponding mean compressive strength at 28 days is 28.7 MPa. The specified yield strength of the reinforcement is 414 MPa and the mean is 465 MPa.

- Compressive strength of the concrete increases during the first 10 years, but does not change thereafter. According to Ref. 53, and assuming the concrete and curing conditions are similar to this study, the mean compressive strength (in units of MPa) at time t is evaluated by

$$E[f_c(t)] = \begin{cases} 15.51 + 3.95 \ln t, & t < 10 \text{ years} \\ 47.91, & t \geq 10 \text{ years} \end{cases} \quad (13)$$

in which t is in days. The concrete section area decreases with time as damage accumulates. Other engineering properties of the wall are assumed to be time-invariant.

The mean degradation in shear strength of the wall in which expansive aggregate reactions occur is illustrated in Fig. 6. Also illustrated in the figure is the mean degradation in wall shear strength evaluated when the cumulative effect of multiple defects in a section is ignored. The gain in shear strength due to the continuous hydration of concrete more than compensates for the strength degradation due to the loss of section area up to about 50 years. Ignoring the cumulative effect of defects provides an overly optimistic estimate of degradation.

The failure probabilities and the hazard functions associated with the strength degradation illustrated in Fig. 6 are presented in Figs. 7 and 8, respectively. As noted Ref. 44, the increase in failure probability due to the strength degradation is small because of the large variability in earthquake load intensity. However, the hazard function increases rapidly after about 50 years when the cumulative effect of defects is considered.

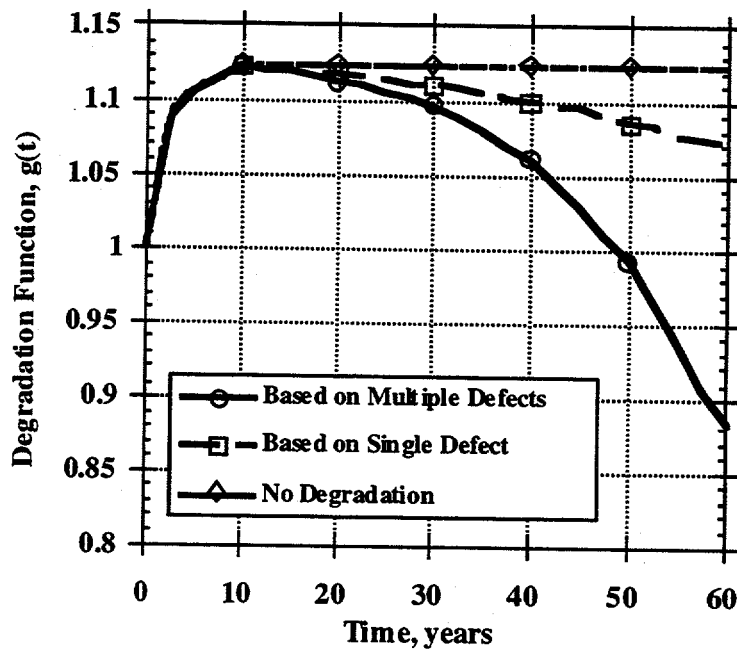


Fig. 6 Mean degradation function of wall in shear without repair.

The mean degradation in flexure/compression strength of the wall is more sensitive to the loss of the outer cross-sectional area than is the shear strength, as shown in Fig. 9. Since the loss of the outer part of the wall leads to a reduction in the internal moment arm, the flexural strength degrades more rapidly than the shear strength, which decreases linearly as a function of the loss of cross-section area. Thus, if the governing limit state of the wall is flexure, special attention should be given to the potential for degradation when performing a condition assessment.

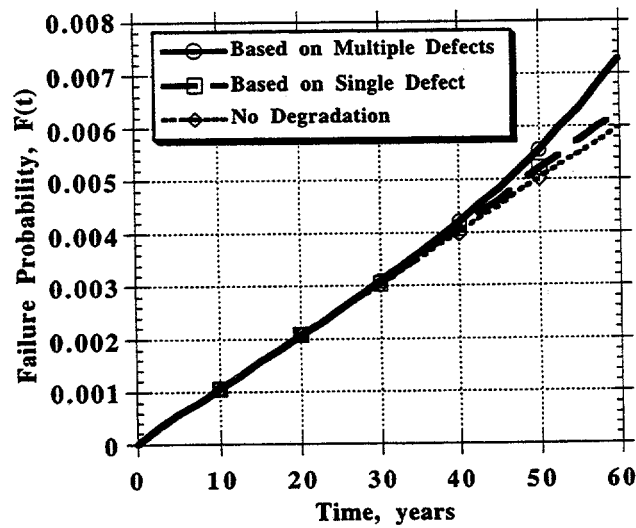


Fig. 7 Failure probability of wall in shear without repair.

3.4.4 Condition Assessment and In-Service Inspection

Forecasts of reliability of the type illustrated in Figs. 4 and 7 enable the analyst to determine the time period beyond which the desired reliability of the structure cannot be ensured. At such a time, the structure should be inspected. Intervals of inspection and maintenance that may be required as a condition for continued operation can be determined from the time-dependent reliability analysis. In-service inspection and maintenance are a routine part of managing aging and deterioration in many engineered facilities; work already has been initiated to develop policies for offshore platforms and aircraft.^{54,55}

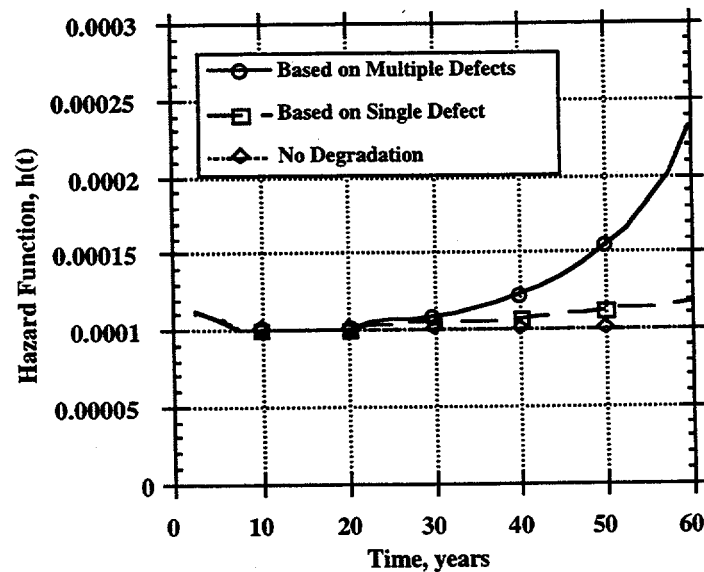


Fig. 8 Hazard function of wall in shear without repair.

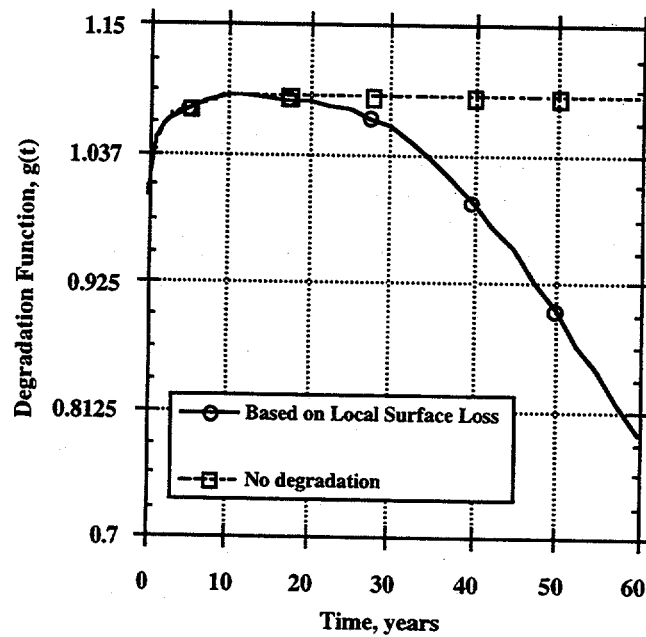


Fig. 9 Mean degradation function of wall in flexure/compression.

When a structure is inspected and/or repaired, something is learned about its in-service condition that enables the probability distribution of strength to be updated. The density function of strength, based on prior knowledge of the materials in the structure, construction, and standard methods of analysis, is indicated by $f_R(r)$. Scheduled inspection, maintenance and repair cause the characteristics of strength to change; this is denoted by the (conditional) density $f_R(r|B)$, in which B is an event dependent on in-service inspection. Information gained from the inspection usually involves several structural variables including dimensions, defects, and perhaps an indirect measure of strength or stiffness. If these variables can be related through event B , then the updated density of R following in-service inspection is,

$$f_R(r|B) = P[r < R \leq r + dr, B] / P[B] = c K(r) f_R(r), \quad (14)$$

in which $f_R(r)$ is termed the prior density of strength, $K(r)$ is denoted the likelihood function, and c is a normalizing constant. The time-dependent reliability analysis then is re-initialized following in-service inspection/repair using the updated $f_R(r|B)$ in place of $f_R(r)$. The updating causes the hazard function (e.g., Fig. 8) to be discontinuous.

Uncertainties in methods of in-service inspection/repair affect the density $f_R(r|B)$. Using a combination of methods usually is more effective from a reliability point of view than using one method. As noted by Refs. 20 and 43, when there are limited resources, it often is most effective to select a few critical safety elements and concentrate on them. Optimal intervals of inspection and repair for maintaining a desired level of reliability can be determined based on minimum life cycle expected cost considerations. Preliminary investigations of such policies have found that they are sensitive to relative costs of inspection, maintenance, and failure.^{40,42} If the cost of failure is an order (or more) of magnitude larger than inspection and maintenance costs, the optimal policy is to inspect at nearly uniform intervals of time. However, additional research is required before such policies can be finalized as part of an aging management plan.

4. CONCLUSIONS

Summarized below are major conclusions that were provided in the final report for this program.⁴

- The performance of the concrete structures in NPPs has been good. The majority of the identified problems initiated during construction and were corrected at that time. However, as these structures age, incidences of degradation due to environmental stressor effects may threaten their durability. Items of note would be corrosion of steel reinforcement following carbonation of the concrete or ingress of chloride ions, excessive loss of prestressing force, leaching of concrete, and leakage of post-tensioning system corrosion inhibitor through cracks in the concrete.
- Techniques for detecting the effects of environmental stressors are sufficiently developed to provide qualitative data. Areas of concern include massive members that contain large quantities of steel reinforcement (e.g., the basemat) and members that are inaccessible (e.g., portions of the steel pressure boundary that are embedded in concrete). Despite the limitations associated with many of the techniques, their proper use and application provides vital input for assessing the structural condition of reinforced concrete members. Frequently, increased confidence in results can be provided by using a combination of methods.
- Methods for use in conducting condition assessments of reinforced concrete structures are fairly well established and generally start with a visual examination of the structure's surfaces. Condition assessments provide an effective aging management tool in that when a discontinuity is detected, a maintenance activity can be implemented to prevent the discontinuity from becoming a defect that requires a major repair. To be of most use, the condition assessments should be conducted at regular intervals. Established condition assessment methods, however, have been application-specific (e.g., parking structure decks). Few standards or criteria are available for interpreting the results obtained from the condition assessments. Due to the importance of condition assessments in effectively managing aging of structures and the likelihood for incidences of degradation to increase as the NPP structures age, it seems prudent that condition assessments of these structures should be conducted periodically. Structures identified to be of high safety significance and potentially at risk should receive the most detailed and frequent inspections. The inspection interval for future inspections could be increased based on a proven performance history. Evaluation of structures having limited accessibility for visual examinations or conduct of other nondestructive evaluations could start with an indirect approach such as monitoring the structure's ambient environment to determine if it is potentially aggressive. More detailed examinations would be required if the environment is found to be potentially aggressive.
- Techniques for repair of concrete structures are well established and when properly selected and applied are effective. At present no codes or standards are available for repair of reinforced concrete structures, although some are being developed. Criteria that may be used to determine when a repair action should be implemented are not available (i.e., parameters that relate damage state such as crack width to environmental exposure). Data on the long-term effectiveness or durability of remedial measures is required. Effective implementation of a repair strategy requires knowledge of the degradation mechanisms and the environment of the structure at the macro and micro level, proper preconditioning of the structure to be repaired, correct choice of repair technique and material, and workmanship of good quality.
- A reliability-based methodology has been developed that can be used to facilitate quantitative assessments of current and future structural reliability and performance of concrete structures in NPPs. The methodology is able to take into account the nature of past and future loads, and randomness in strength and in degradation resulting from environmental factors. The methodology can be used as a basis for selecting appropriate periods for continued service and/or determining optimum intervals and extent of inspection and repair activities. Inspection/repair strategies can be developed to minimize expected future cost while keeping the failure probability of the structure at or below an established target failure probability during its anticipated service period. Implementation and extension of the method to realistic condition assessments is difficult due to a lack of supporting quantitative data on strength degradation models, including initiation and rate of

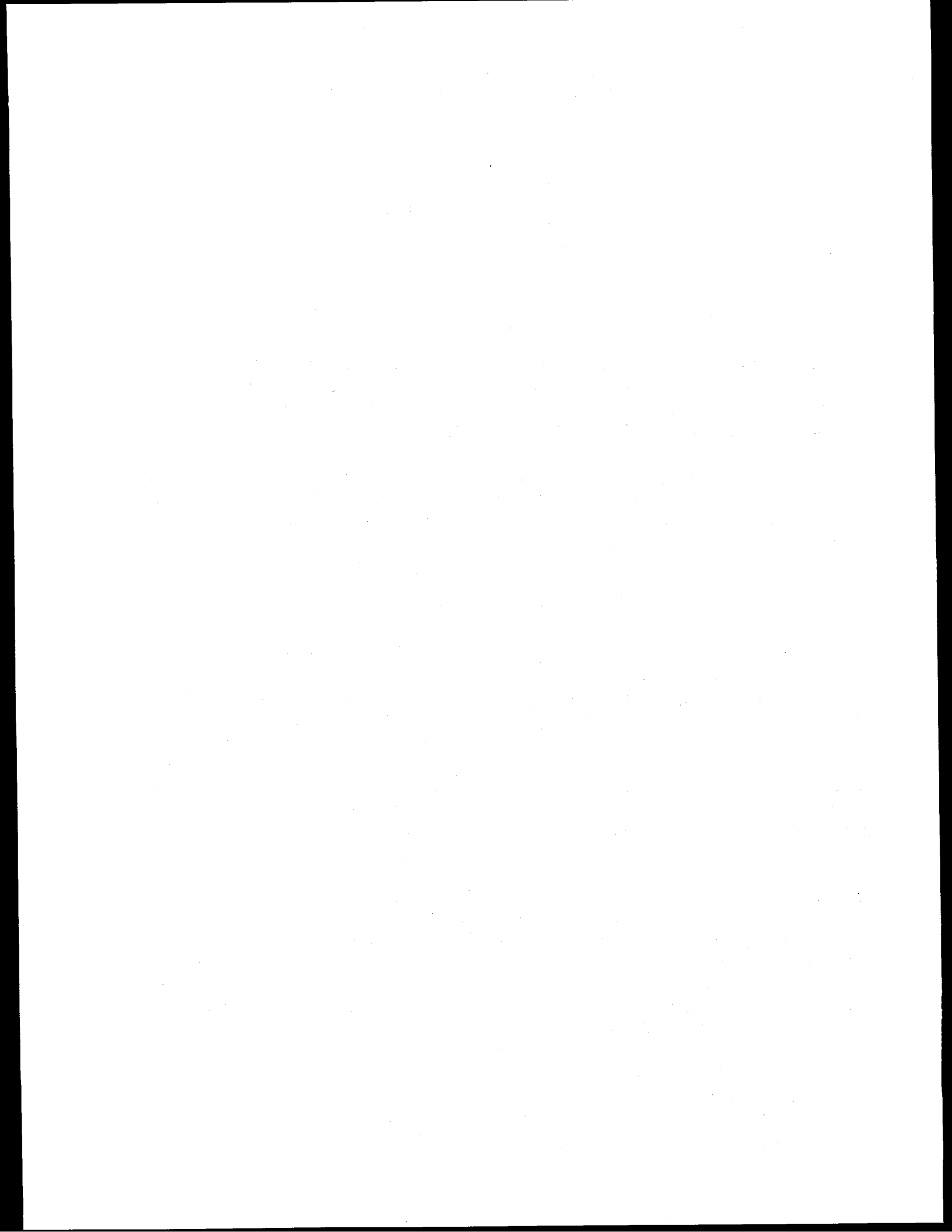
damage growth, the mean occurrence rate of local events and cumulative density function of the intensity of time-varying loads. Validation of these reliability models through application to laboratory or prototypical structures would be desirable.

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TIME-DEPENDENT RELIABILITY ANALYSIS AND CONDITION ASSESSMENT OF STRUCTURES

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ABSTRACT

Structures generally play a passive role in assurance of safety in nuclear plant operation, but are important if the plant is to withstand the effect of extreme environmental or abnormal events. Relative to mechanical and electrical components, structural systems and components would be difficult and costly to replace. While the performance of steel or reinforced concrete structures in service generally has been very good, their strengths may deteriorate during an extended service life as a result of changes brought on by an aggressive environment, excessive loading, or accidental loading. Quantitative tools for condition assessment of aging structures can be developed using time-dependent structural reliability analysis methods. Such methods provide a framework for addressing the uncertainties attendant to aging in the decision process.

1. INTRODUCTION

Safety-related structures in nuclear power plants (NPPs) may be exposed to aggressive service and environmental effects that may cause their strength and stiffness to deteriorate over a 40-year service life. Among the factors having the potential to cause structures to deteriorate are corrosion of structural steel or steel reinforcement; damage to concrete from chemical attack or expansive aggregate reactions; fatigue/fracture from repetitive loads; and elevated temperature creep and irradiation effects. Such aging effects may warrant periodic in-service inspections, structural condition assessments and repair as a condition for continued service. Such condition assessment of existing structural components and systems in NPPs generally would be aimed at providing quantitative evidence of continued acceptable performance during a future service period.

Facilities management policies for aging structures require a set of criteria and quantitative methods for assessing fitness for continued service. These quantitative tools should integrate data on design, material and structural degradation, environmental factors and nondestructive evaluation (NDE) technology into decision tools that provide quantitative measures of performance under current and projected future service conditions. Sources of uncertainty arising from variations in initial structural conditions and service loads, lack of in-service inspection and repair records, limitations in nondestructive evaluation (NDE) technologies, and uncertainties in the effectiveness of repair methods should be reflected in these criteria.

2. TIME-DEPENDENT RELIABILITY ANALYSIS

Research in progress, supported in part by the Oak Ridge National Laboratory and the US Nuclear Regulatory Commission, is aimed at: identifying mathematical models to evaluate structural degradation; recommending statistically-based sampling plans for nondestructive evaluation, and; developing methods to assess the probability that structural capacity has not degraded or will not degrade during a future service period (Naus, et al, 1996a; Naus, et al, 1996b; Ellingwood, et al, 1996; Bhattacharya and Ellingwood, 1996a; Bhattacharya and Ellingwood, 1996b). Although the physics of common damage mechanisms are reasonably well understood, quantitative evaluation of their effects on time-dependent structural behavior often is difficult. Moreover, there are large uncertainties in the process, due to both inherent randomness and lack of information, that can best be handled within a probabilistic framework. Some of the basic concepts in the time-dependent reliability analysis are summarized in the following.

The probability that a structural component or system attains a particular limit state can be determined if the probability distributions of the structural resistance and loads are known. If the margin of safety, M , is defined as,

$$M = R - S \quad (1)$$

in which R = structural resistance and S = structural action due to the loads, the probability of failure is obtained from,

$$P_f = \int_0^{\infty} F_R(x) f_S(x) dx \quad (2)$$

in which $F_R(x)$ and $f_S(x)$ are cumulative distribution function (CDF) of R and probability density function (PDF) of S , respectively. The (instantaneous) reliability is simply $1 - P_f$.

When the engineering material properties change due to aging, the structural resistance becomes time-dependent. Structural loads also are random functions of time. Thus, the margin M in Eqn 1 is a function of time, t , and Eqn 2 provides the probability of failure at t . This instantaneous measure of reliability may be useful for certain applications, e.g., in a safety margins assessment. However, for service life prediction and for scheduling inspections and maintenance, the reliability during a prescribed interval of time, e.g., $(0, t_1)$ for time to first inspection or (t_i, t_{i+1}) for time between the i th and $(i+1)$ th inspections, may provide a better basis for decision-making.

The probability that a structural system or component survives during an interval of time, say $(0, t)$, is defined by the reliability function, $L(0, t)$. If only extreme environmental or abnormal loads that occur rarely are of interest for structural safety assessment, the load process can be modeled as a sequence of discrete load events $S_1, S_2, \dots$, whence,

$$L(0,t) = P[R(t_1) > S_1, R(t_2) > S_2, \dots] \quad (3)$$

in which $t_1, t_2, \dots$ are times at which the loads occur. If the occurrence of these events is modeled as a Poisson process, with mean rate λ , it can be shown (Ellingwood and Mori, 1993) that,

$$L(0,t) = \int_0^\infty \exp(-\lambda t [1 - t^{-1} \int_0^t F_S(gr) dt]) f_R(r) dr \quad (4)$$

in which g = fraction of initial strength, R , remaining at time t . The probability of failure (attaining a specified performance limit) during interval $(0,t)$ is $F(0,t) = 1 - L(0,t)$.

The reliability measures L and F can be determined from the hazard function, $h(t) dt$, of the structural deterioration process, defined as the probability of failure within time interval $(t, t+dt)$, given that the component or structure has survived up to time, t . The functions L and h are related by (Ellingwood and Mori, 1993),

$$L(t_{i-1}, t_i) = \exp[-\int_{t_{i-1}}^{t_i} h(x) dx] \quad (5)$$

Applying Eqn 5 repetitively, structural reliability for a succession of intervals (perhaps between scheduled inspections) is,

$$L(0,t) = \prod L(t_{i-1}, t_i) \exp[-\int_{t_1}^t h(x) dx] \quad (6)$$

in which $t_{i-1} = 0$ when $i = 1$. Integration of $h(t)$ provides a measure of reliability for any service interval of interest.

The hazard and reliability functions must be determined from the stochastic models of deterioration and external loads for a particular mechanism of structural aging. The reliability function invariably is monotonically decreasing with time, regardless of whether aging occurs, since the probability of an extreme load occurring in the interval $(0,t)$ increases with t . However, the hazard function provides a clearer picture of the impact of aging on structural reliability. For purely chance failures, $h(t)$ is constant. When strength deteriorates as a result of structural aging, $h(t)$ characteristically increases with time, even if the the load process remains statistically stationary.

3. ROLE IN IN-SERVICE INSPECTION AND MAINTENANCE

Estimates of time-dependent reliability using Eqns 3 - 6 enable one to determine the time or in-service interval beyond which the desired reliability of the structure cannot be ensured. At such a time, the structure should be inspected and its condition evaluated. In-service inspection and maintenance (ISI/M) operations, although often ad hoc or based on nonstructural considerations (e.g., at refueling outages), are a routine part of managing aging in many structures. The information gained from inspection usually involves several structural variables, such as dimensions or defect sizes, and often an indirect indication of strength or stiffness such as ultrasonic pulse velocity (Carino, 1994).

There are significant uncertainties associated with ISI/M, which ought to be reflected in the time-dependent reliability analysis. For example, while some types of flaws are detected relatively easily (e.g., surface cracks in weldments), others may be more difficult to identify (expansive aggregate reactions within concrete). The probability that the defect will not be detected is depicted by the detection curves $d(x)$ illustrated in Figure 1, which take the form of complementary cumulative distribution functions. In Figure 1, x = generalized flaw size parameter (crack width, depth of corrosion, extent of internal deterioration of concrete). The shape of $d(x)$ depends on the NDE technology employed. Below the threshold of detection, x_{th} , which is related to the resolution of the NDE technology, the flaw remains undetected. NDE typified by $d_3(x)$ in Figure 1, with its lower threshold of nondetection, generally would be more costly than that typified by $d_1(x)$. Moreover, once a flaw is detected, there may be error in measuring flaw size due to lack of precision or difficult field conditions. There also is the possibility that NDE indicates a flaw when none is present (a so-called false call), leading to costly and unnecessary repair and (in the case of rewelding, for example) possible further damage.

In reliability terms, ISI/M leads to a re-evaluation of the CDF of resistance, margin, and hazard function. Beneficial maintenance causes $h(t)$ to decrease and, in turn, increases the reliability estimated by Eqns 4 - 6 with respect to a future service or inspection interval. Changes in $h(t)$ or $L(0,t)$ depend on the probability of detecting strength-reducing defects of a given size, error in defect measurement, and the (hopefully) beneficial strengthening from repair; these, in turn, depend on the characteristics of the NDE technology and repair methods selected.

The results of the inspection can be represented as event H , which involves inspection and structural variables. The updated CDF of resistance, R , or margin of safety, M , can be obtained from Bayes theorem:

$$F_R(r|H) = P[R \leq r \text{ and } H]/P[H] \quad (7)$$

$$P[M < 0|H] = P[M \leq 0 \text{ and } H]/P[H] \quad (8)$$

in which $F_R(r|H)$ and $P[M < 0|H]$ are the CDF of R or M immediately following inspection. If the

structure subsequently is repaired, the CDF of strength or margin again is updated using Eqns 7 or 8 in which the conditioning event H now depends on the effectiveness of the repair operation. Evaluating the joint probabilities on the right hand side of Eqns 7 and 8 often is nontrivial, but can be attacked using efficient Monte Carlo techniques (e.g., Melchers, 1989).

4. ILLUSTRATION OF SERVICE LIFE PREDICTION

Two examples of time-dependent reliability analysis are presented in this section: a reinforced concrete slab and a steel cylindrical shell. Simple cases are considered to illustrate the basic ideas. Extensions to more complex structures may impose a computational burden (e.g., nonlinear finite element analysis) but are conceptually similar. The probability models of loads and strength used in these illustrations are summarized in Table 1. Their basis is described elsewhere (Hwang, et al, 1987; Ellingwood and Mori, 1993).

4.1 Reinforced concrete slab

As a first example, a concrete slab was designed using the requirements for flexural strength in ACI Standard 349 ("Code," 1990):

$$0.9 M_n = 1.4 D + 1.7 L \quad (9)$$

in which M_n is code-specified flexural strength, and D and L are dead and live load (e.g., Minimum, 1995). The specified compression strength of the concrete is 28 MPa (4000 psi) while the specified yield strength of the reinforcement is 414 MPa (60 ksi). The dead load is modeled as a random variable that is constant in time while the live load occurrences are modeled as a Poisson pulse process with mean rate (load changes) of 0.5/yr. The strength of the slab changes in time. As the concrete matures, the mean compressive strength increases according to

$$f_c(t) = 15.5 + 3.95 \ln t \leq 47.9 \quad (10)$$

(t is measured in days), from 28.7 MPa (4,150 psi) at 28 days to 47.9 MPa (6,950 psi) at 10 years. At the same time, loss of section due to active corrosion is modeled as,

$$x(t) = C (t - T_i)^m \quad (11)$$

in which T_i = induction period required for active corrosion to initiate and C and m = experimental constants determined from the data. In this illustration, the time to initiate active corrosion is taken as a lognormal random variable, with a mean of 10 yr and coefficient of variation (COV) of 0.30;

the time-order parameter, m , is assumed to be deterministic and equal to 1.0; and the rate parameter, C , is lognormal, with a mean $30 \mu\text{m}$ (corresponding to a mean loss of rebar section in 40 yr of about 15%) and a COV of 0.5.

A 60-year service life with three alternative ISI/M strategies is considered: (1) Inspect the slab fully at 30 years with a NDE technique capable of detecting defects causing a 1 percent (or more) reduction in strength (denoted $x_{th} = 0.01$), and fully repair; (2) Inspect the slab at 20, 30, 40 and 50 years with a NDE method capable of detecting only defects causing an 8% (or more) decrease in strength (denoted $x_{th} = 0.08$), and fully repair; and (3) Do not inspect or repair the slab. Sample functions of the stochastic strength processes are illustrated in Figure 2.

The estimated time-dependent (cumulative) probabilities $F(0,t)$ for these three policies are computed by Monte Carlo simulation and are illustrated in Figure 3. A performance goal that the probability of failure during the 60-year service life be less than, e.g., 1.5×10^{-4} , may be achieved by several ISI/M policies. Selecting the appropriate policy requires a comparative evaluation of the discounted present worth costs associated with each policy.

4.2 Steel cylindrical shell

As a second example, we examine deterioration of a cylindrical steel shell due to uniform corrosion. This shell is fabricated from pressure vessel grade carbon steel A516/70, with a nominal yield strength of 262 MPa (38 ksi) and ultimate strength 483 MPa (70 ksi). The shell has a radius of 17 m (55 ft) and is designed for a pressure of $P_a = 276 \text{ kPa}$ (40 p_{sig}) according to the requirement,

$$S_{mc} > D + P_a \quad (12)$$

in which $S_{mc} = 134 \text{ MPa}$ (19.5 ksi) is the allowable stress for A516/70 steel and D, P_a = stresses caused by dead load and pressure. The resulting uniform thickness is 35 mm (1 3/8 in).

The yielding limit state is defined as,

$$P_y(t) - P(t) = 0 \quad (13)$$

in which $P_y(t)$ = pressure corresponding to first yield and $P(t)$ is the magnitude of the pressure at time t . Since the shell does not lose its ability to resist further pressure build-up when $P_y(t)$ is reached, this is a conservative limit state. The occurrence of pressures is modeled as a Poisson process, with a mean rate 0.0017/yr; the statistics of pressure intensity are given in Table 1 (Hwang, et al, 1987). The time to initiate corrosion is assumed to be lognormal, with mean of 10 yr and COV of 0.30, as in the previous example. Three different mean corrosion rates are considered to illustrate the impact of this parameter on time-dependent reliability: $C = 0, 230 \mu\text{m}$, and $600 \mu\text{m}$, spanning the range from no corrosion to very rapid corrosion; the latter rate is well above the rates that would be typical for

uniform corrosion in NPPs (Ellingwood, et al, 1996). The time-order parameter in Eqn 11 in this illustration is equal to 0.7.

Figure 4 illustrates the hazard function and (cumulative) probability of net section yielding as a function of elapsed times up to 60 years. If the corrosion loss is neglected, $h(t)$ remains nearly constant, implying that the instantaneous failure probability does not increase with time. The effect of corrosion on reliability on both $h(t)$ and $F(t)$ is apparent. However, because of the mean rate of occurrence of $P(t)$ is very small (0.0017/yr), the corrosion has less than an order-of-magnitude effect on cumulative probability after 60 years. This is in contrast to the findings in the first illustration, where the load occurrence rate was higher (0.5/yr).

5. CONCLUSIONS

Time-dependent structural reliability analysis integrates data on design requirements, material and structural degradation and damage accumulation, environmental factors and NDE technology into facility management and decision tools. Stochastic models of damage mechanisms, residual strength, and service loads enable the failure probability of a component to be evaluated as a function of time. Reliability-based decision tools provide quantitative measures of reliability under current and projected future service conditions.

Analyses performed for structures subjected to several of the damage mechanisms identified above (Mori and Ellingwood, 1993; Naus, et al, 1996a; Ellingwood, et al, 1996) have led to several general observations and conclusions. Corrosion appears to have the most significant impact on reliability of reinforced concrete structures or steel containments and liners. Effects of fatigue, creep and irradiation generally are not significant. Neglect of aging entirely can lead to an order-of-magnitude difference in estimated probability of structural failure during a service life of 40 years. However, this difference may not translate into a comparable change in facility risk. In contrast to findings for some mechanical and electrical components reported elsewhere, the conditional failure rate functions for structural components generally increase nonlinearly with time. An examination of system reliability shows that it is sufficient to focus attention on only a few key components in probability-based condition assessment. Finally, when consequences of failure are severe in comparison to cost of in-service inspection and maintenance, a near-optimum strategy is to inspect at uniform intervals during plant service.

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Table 1.

Load process parameters and statistical models of strength

<u>Parameter</u>	<u>Mean</u>	<u>COV</u>	<u>CDF</u>	<u>Rate(yr⁻¹)</u>
Dead load	1.0D	0.07	Normal	-
Live load	0.4L	0.50	Type I	0.5
Pressure load	0.8Pa	0.20	Type I	0.0017
Flexural strength of RC slab (t = 0)	1.12Mn	0.14	Lognormal	-
Yield strength of A516/70 steel	1.10Fy	0.07	Lognormal	-

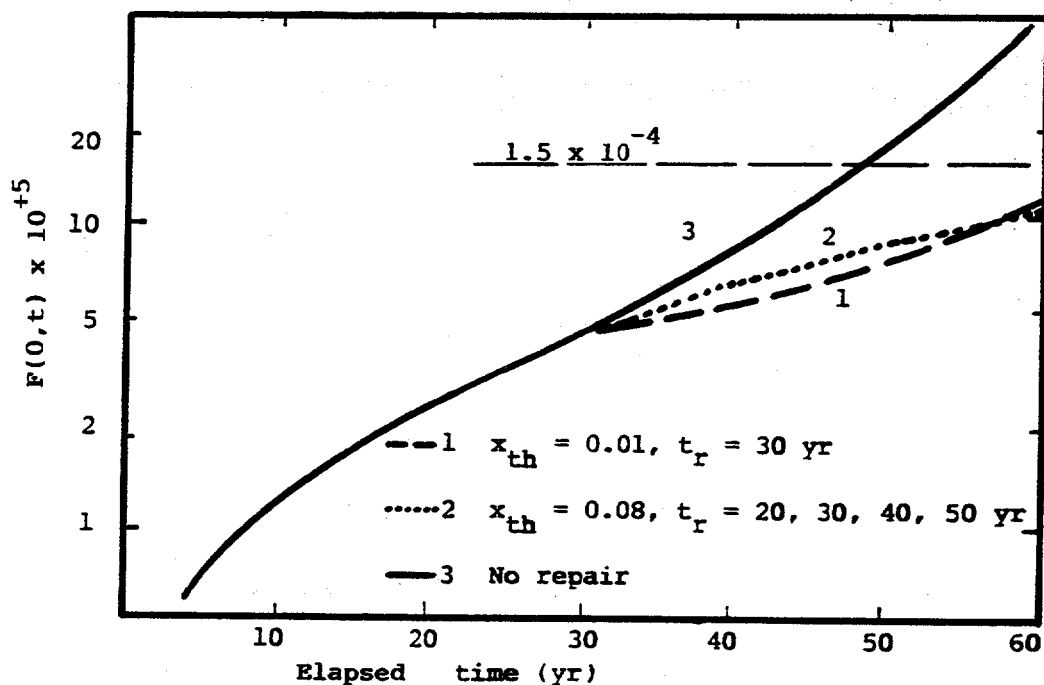


Figure 3. Probability of flexural failure of reinforced concrete slab

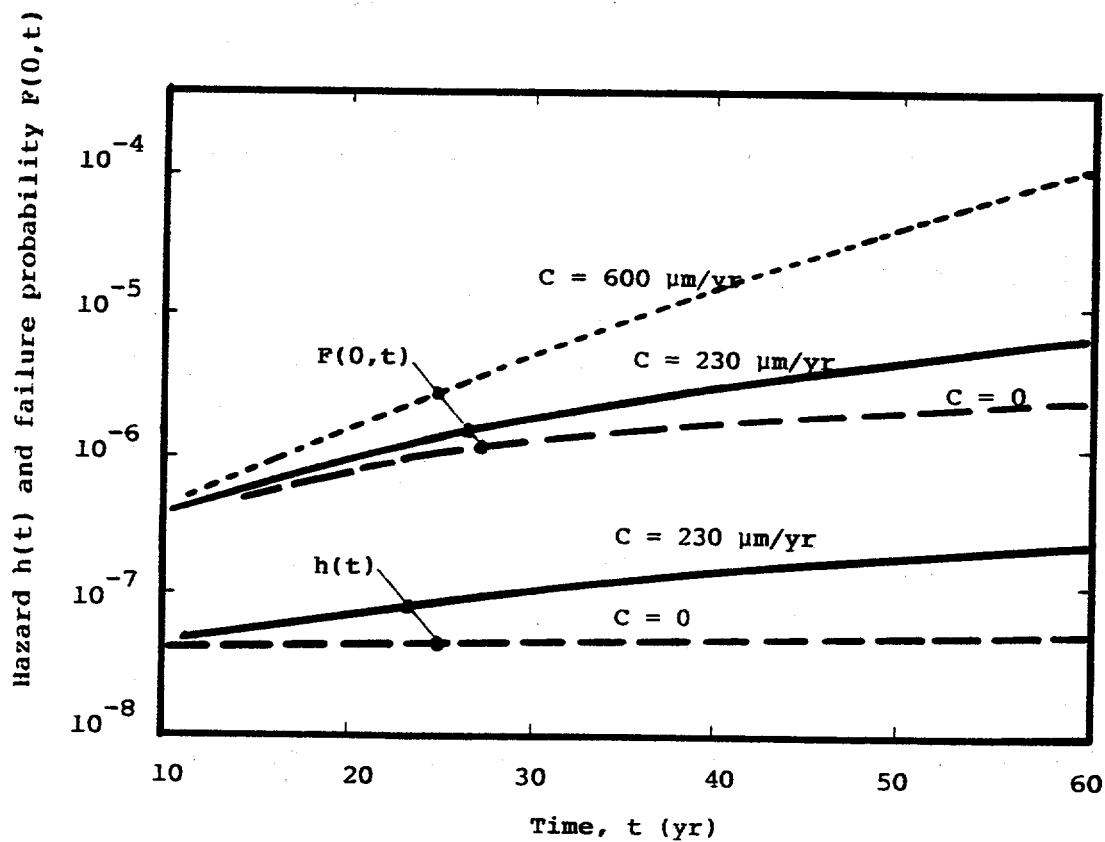


Figure 4. Probability of yielding of steel cylindrical shell

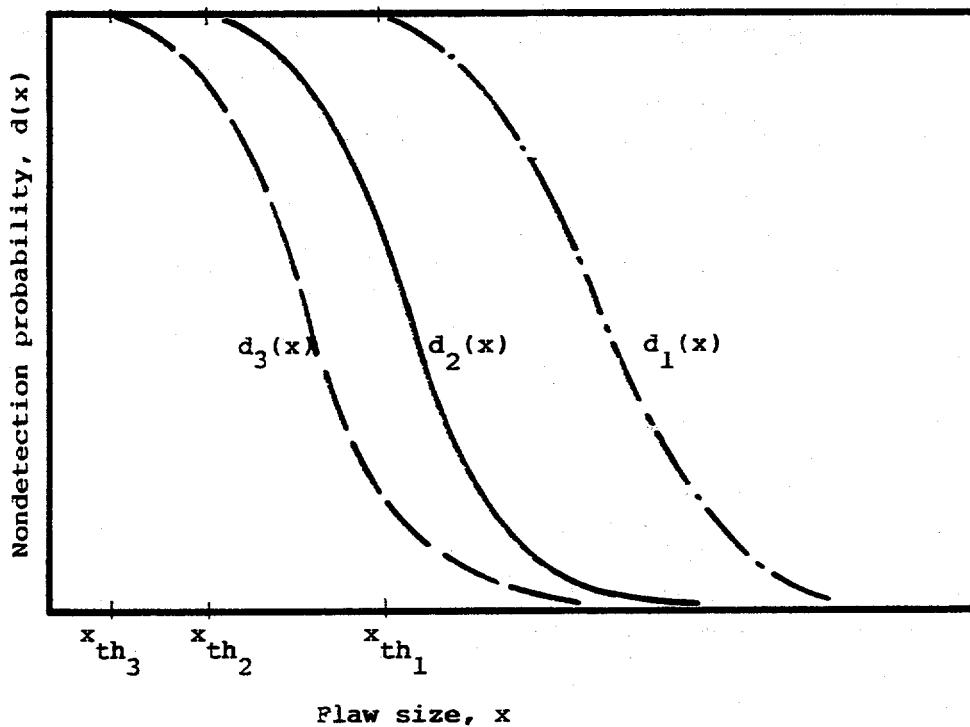


Figure 1. Probability that flaw is not detected

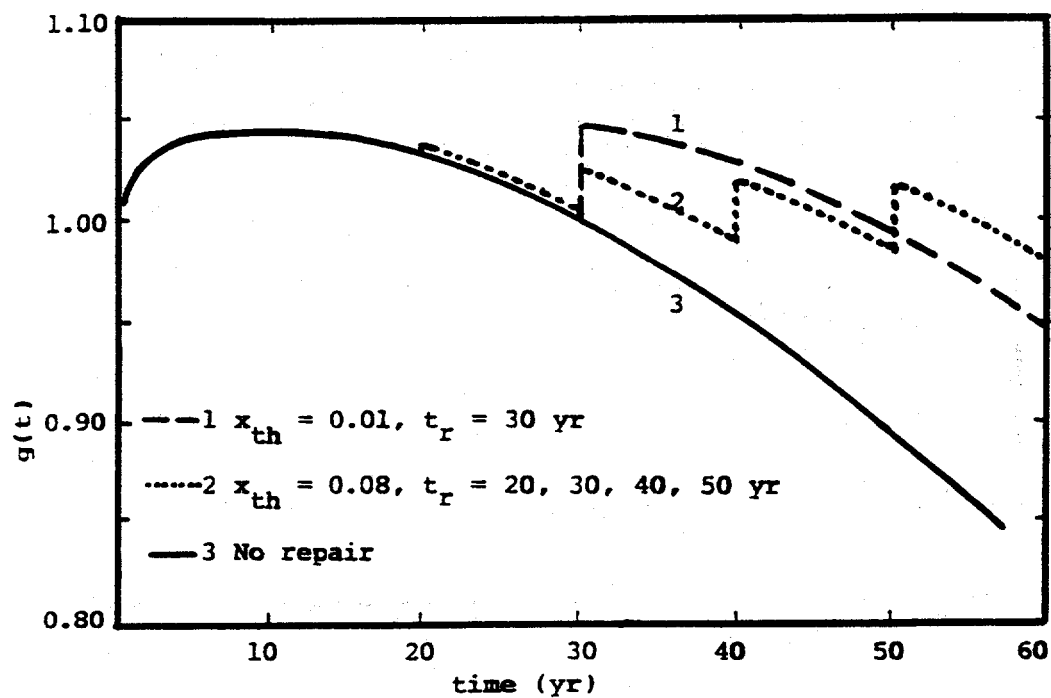


Figure 2. Sample functions of strength process

AGING OF THE CONTAINMENT PRESSURE BOUNDARY IN LIGHT-WATER REACTOR PLANTS

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ABSTRACT

Research is being conducted by the Oak Ridge National Laboratory to address aging of the containment pressure boundary in light-water reactor plants. The objectives of this work are to (1) identify the significant factors related to occurrence of corrosion, efficacy of inspection, and structural capacity reduction of steel containments and liners of concrete containments, and to make recommendations on use of risk models in regulatory decisions; (2) provide NRC reviewers a means of establishing current structural capacity margins for steel containments, and concrete containments as limited by liner integrity; and (3) provide recommendations, as appropriate, on information to be requested of licensees for guidance that could be utilized by NRC reviewers in assessing the seriousness of reported incidences of containment degradation. In meeting these objectives research is being conducted in two primary task areas – pressure boundary condition assessment and root-cause resolution practices, and reliability-based condition assessments. Under the first task area a degradation assessment methodology was developed for use in characterizing the in-service condition of metal and concrete containment pressure boundary components and quantifying the amount of damage that is present. An assessment of available destructive and nondestructive techniques for examining steel containments and liners is ongoing. Under the second task area quantitative structural reliability analysis methods are being developed for application to degraded metallic pressure boundaries to provide assurances that they will be able to withstand future extreme loads during the desired service period with a level of reliability that is sufficient for public safety. To date, mathematical models that describe time-dependent changes in steel due to aggressive environmental factors have been identified, and statistical data supporting their use in time-dependent reliability analysis have been summarized. The analysis of steel containment fragility has been described and simple illustrations of the impact on reliability of structural degradation provided. Also, the role of nondestructive evaluation in time-dependent reliability analysis, both in terms of defect detection and sizing, has been examined.

1. INTRODUCTION

1.1 Background

As of March 1995 there were 109 nuclear power plants (NPPs) licensed for commercial operation in the United States with 1 reactor still under construction and 5 reactors partially completed, but under a deferred construction schedule.^{1*} The Atomic Energy Act (AEA) of 1954 limits the duration of operating licenses for most of these reactors to a maximum of 40 years. Forty-nine of these reactors have been in commercial operation for 20 or more years. Expiration of the operating licenses for these reactors will start to occur in the year 2000 when Big Rock Point's license expires. Under current economic, social, and political conditions in the United States, the

* References are collected and provided at the end of this paper.

prospects for early resumption of building of new NPPs to replace lost generating capacity are very limited.² In some areas of the country it may be too late because of the 10 to 15 years required to plan and build replacement power plants. Continuing the service of existing NPPs through a renewal of their initial operating licenses provides a timely and cost-effective solution to the problem of meeting future energy demand.

The 40-year term on the duration of an operating license provided in the AEA of 1954 apparently was based on various financial considerations (e.g., bond maturity) and not on safety or technical concerns. No technical information was presented to suggest that the NPPs would become unsafe if they were to operate after 40 years.³ In fact, the AEA permits the renewal of operating licenses. Paragraph 50.51 of Part 10 of the *Code of Federal Regulations (CFR)*⁴ implements the authority; however, no prior standards have been provided for preparing or evaluating license renewal applications.

A major concern in the evaluation of applications to renew initial operating licenses is that the capacity of the safety-related systems to mitigate extreme events has not deteriorated unacceptably due to either aging or environmental stressor effects during their previous service history. Major mechanical and electrical equipment items in a plant could in all likelihood be replaced, if necessary. However, replacement of the containment pressure boundary (i.e., steel containment or liner of reinforced concrete containment) would not be economically feasible. Approval for service life extension must be supported by evidence that these systems will continue to be capable of withstanding potential future extreme events.

1.2 Objective

The objectives of this work are to (1) understand the significant factors relating occurrence of corrosion, efficacy of inspection, and structural capacity reduction of steel containments and liners of concrete containments, and to make recommendations on use of risk models in regulatory decisions; (2) provide NRC reviewers a means of establishing current structural capacity margins for steel containments, and concrete containments as limited by liner integrity; and (3) provide recommendations, as appropriate, on information to be requested of licensees for guidance that could be utilized by NRC reviewers in assessing the seriousness of reported incidences of containment degradation.

1.3 Application of Results

Results being developed under this activity will provide background data and information for use by reviewers or licensees as part of the assessment to determine if the intent of the license renewal (*10 CFR Part 54*) and maintenance (*10 CFR Part 50.65*) rules are being met with respect to the containment pressure boundary. Specific applications include evaluations of the (1) in-service inspection techniques and methodologies that have been utilized as part of an overall program to ensure that the containment pressure boundary will continue to provide the required safety margins, and (2) root-cause resolution practices that have been applied to restore containment pressure boundary components that have been damaged or degraded in service. Methods established under the reliability-based condition assessment activity provide a means of establishing current structures capacity margins or estimating future residual structural capacity margins of the pressure boundary components.

2. CONTAINMENT PRESSURE BOUNDARY COMPONENTS

From a safety standpoint, the containment pressure boundary is one of the most important components in a nuclear power plant (NPP) because it serves as the final barrier to the release of radioactive fission products to the outside environment under postulated accident conditions. Ensuring that the capacity of these components has not deteriorated unacceptably due to either aging or environmental stressor effects is essential to reliable continued service evaluations and informed aging management decisions.

2.1 General Description

Each boiling-water reactor (BWR) or pressurized-water reactor (PWR) unit in the United States is located within a much larger metal or concrete containment that also houses or supports the primary coolant system components. Although the shapes and configurations of the containment can vary significantly from plant-to-plant depending on the nuclear steam supply system vendor, architect-engineering firm, and owner preference, leaktightness is assured by a continuous pressure boundary consisting of nonmetallic seals and gaskets, and metallic components that are either welded or bolted together. Nonmetallic components are used to prevent leakage from pumps, pipes, valves, personnel airlocks, equipment hatches, manways, and mechanical and electrical penetration assemblies. The remaining pressure boundary consists primarily of steel components such as metal containment shells, concrete containment liners, penetration liners, heads, nozzles, structural and nonstructural attachments, embedment anchors, pipes, tubes, fittings, fastenings, and bolting items that are used to join other pressure-retaining components. Each containment type includes numerous access and process penetrations that complete the pressure boundary (e.g., large opening penetrations, control rod drive removal hatch, purge and vent system isolation valves, piping penetrations, and electrical penetration assemblies). More details on information provided below can be obtained from Refs. 5-7.

Except for spherical containments (i.e., Yankee Rowe,* Big Rock Point, and San Onofre 1*) that are exposed to the natural environment but employ extensive coating systems for protection, metal containments are free-standing, welded steel structures that are enclosed in a reinforced concrete reactor or shield building. The reactor or shield buildings are not part of the pressure boundary and their primary function is to provide protection for the containment from external missiles and natural phenomena (e.g., earthquakes, tornadoes, or site-specific environmental events). Thirty-nine of NPPs presently licensed for commercial operation in the United States employ a metal containment. These containments fall into six general categories (1) BWR Mark I, (2) BWR Mark II, (3) BWR III, (4) BWR and PWR spherical, (5) PWR cylindrical with hemispherical top and ellipsoidal base, and (6) PWR cylindrical with hemispherical dome and flat base.

Concrete containments are metal lined, reinforced concrete pressure-retaining structures that in some cases may be post-tensioned. The concrete vessel includes the concrete shell and shell components, shell metallic liners, and penetration liners that extend the containment liner through the surrounding shell concrete. The reinforced concrete shell, which generally consists of a cylindrical wall with a hemispherical or ellipsoidal dome and flat base slab, provides the necessary structural support and resistance to pressure-induced forces. Leaktightness is provided by a steel liner fabricated from relatively thin plate material (e.g., 6.4-mm thick) that is anchored to the concrete shell by studs, structural steel shapes, or other steel products. Seventy of the NPPs that have been licensed for commercial operation in the United States employ either a reinforced concrete (30 plants) or post-tensioned concrete (40 plants) containment.

2.2 Requirements for Design and Construction

The basic laws that regulate the design (and construction) of NPPs are contained in Title 10, "Energy," of the *CFR*⁴ that is clarified by Regulatory Guides (e.g., R.G. 1.29),⁸ NUREG reports, Standard Review Plan (e.g., Concrete Containment),⁹ etc. General Design Criteria 1, "Quality Standards and Records;" 2, "Design Bases for Protection Against Natural Phenomena;" and 4, "Environmental and Dynamic Effects Design Bases," of Appendix A, "General Design Criteria for Nuclear Plants," to *10 CFR Part 50*, "Domestic Licensing of Production and Utilization Facilities," require, in part, that structures, systems, and components be designed, fabricated, erected, and tested to quality standards commensurate with the safety functions to be performed and that they be designed to withstand the effects of postulated accidents and environmental conditions associated with normal operating conditions. General Design Criterion 16, "Containment Design," requires that a reactor containment and associated systems be provided to establish an essentially leaktight barrier against the uncontrolled release of radioactivity to the

* Presently shut-down.

environment and to assure that the containment design conditions important to safety are not exceeded for as long as postulated accident conditions require. General Design Criterion 50, "Containment Design Basis," requires, in part, that the containment structure and associated systems be designed to accommodate, without exceeding the design leakage rate and with sufficient margin, the calculated pressure and temperature conditions resulting from any loss-of-coolant accident. Finally, General Design Criterion 53, "Provisions for Containment Testing and Inspection," requires that the containment be designed to permit (1) appropriate periodic inspection of all important areas, such as penetrations; (2) an appropriate surveillance program; and (3) periodic testing at containment design pressure of the leaktightness of penetrations which have resilient seals and expansion bellows. The General Design Criteria provide essential safety requirements for design and licensing bases. Basic rules for the design and construction of metal and concrete containments are prepared by the American Society of Mechanical Engineers (ASME) and published in the *ASME Boiler and Pressure Vessel Code*.¹⁰

2.2.1 Metal containments

Prior to 1963, metal containments for NPPs were designed according to rules for unfired pressure vessels that were contained in Section VIII of the ASME Code.¹¹ Subsequent metal containments were designed either as Class B vessels or as Class MC components according to rules provided in Section III of the ASME Code. Almost every aspect of metal containment design is addressed by the Code, including methods for calculating required minimum thickness of pressure retaining components. The Code also recognizes that service-related degradation to pressure retaining components is possible, but rules for material selection and in-service degradation are outside its scope. According to the Code, it is the Owner's responsibility to select materials that are suitable for the service conditions and to increase minimum required thickness of the base metal to offset material thinning due to corrosion, erosion, mechanical abrasion, or other environmental effects.

Current rules for construction of metal containments are provided in Section III, Division 1, Subsection NE of the ASME Code. Piping, pumps, and valves that are part of the containment system or that penetrate or are attached to the containment are classified as Class 1 or Class 2 components. These components are covered by rules that appear in other subsections of Section III, Division 1 of the ASME Code.

2.2.2 Concrete containments

Initially, existing building codes such as American Concrete Institute (ACI) Standard 318, *Building Code Rules for Reinforced Concrete*,¹² were used in the nuclear industry as the basis for design and construction of concrete structural members. However, because the existing building codes did not cover the entire spectrum of design requirements and because they were not always considered adequate, the U.S. Nuclear Regulatory Commission (NRC) developed its own criteria for design of seismic Category 1 (i.e., safety related) structures (e.g., definitions of load combinations for both operating and accident conditions). Current requirements for concrete reactor vessels and containments were developed by joint technical committees of the ACI and ASME and first published in 1977.¹³ Supplemental load combination criteria are presented in Section 3.8.1 of the *NRC Regulatory Standard Review Plan*.⁹ Section 3.8.3 of the *NRC Regulatory Standard Review Plan*¹⁴ provides information related to concrete and steel internal structures of steel and concrete containments. Plants that used early ACI codes for design have been reviewed by the USNRC through the Systematic Evaluation Program to determine if there were any safety concerns.¹⁵

Current rules for construction of concrete containments are provided in Section III, Division 2, Subsection CC of the ASME Code. Parts or appurtenances that are not backed by structural concrete for load-carrying purposes are covered by appropriate rules that appear in Section III, Division 1 of the ASME Code. Rules for design and construction of the metal liner that forms the pressure boundary for the reinforced concrete containments are found in ASME Section III, Division 1, Subsection NE of the ASME Code.

2.3 Construction Materials

All containments include pipes, electrical penetration assemblies, equipment hatches, manways, air locks, etc., as part of the pressure boundary. These components generally are either welded or bolted to the liners and shells and typically have compositions and properties that are significantly different from those of the liner and shell materials. Leaktightness of the containment pressure boundary is provided by a combination of nonmetallic seals and gaskets, and metallic components that are either welded or bolted together. The metallic components of the containment pressure boundary (i.e., metallic containment and liner of reinforced concrete containments) and assessing the presence and significance of any degradation that might occur are of primary interest in this study.

The ASME Code only permits the use of certain materials for fabrication of containment pressure boundary components. These materials must conform to ASME or American Society for Testing and Materials (ASTM) specifications. Section II, Parts A and D of the ASME Code provide specifications and property values for ferrous materials that are acceptable for use. The ASME Code also specifies which grade, class, or type of steel is permitted for a particular application. Tables 2.1 and 2.3 of Ref. 16 provide a listing of material specifications permitted for construction of metal containments and concrete containment liners. Although the list of acceptable materials provided in this reference is fairly extensive, metal containments have primarily been fabricated of ASME SA-516 (Gr. 60 or Gr. 70), ASTM A 212 (Gr. B), and ASME SA-537 (Gr. B) materials. Mark III free-standing steel containments primarily utilize ASME SA-516 (Gr. 70) material for construction, with the shell plate in the suppression pool clad with ASME SA-240 (Type 304) stainless steel to avoid contact of the carbon steel plate with water. The steel liner plate that acts as a leaktight barrier for the reinforced concrete containments has primarily been fabricated from ASME SA-36, ASME SA-285 (Gr. A or Gr. C), ASME SA-442 (Gr. 60), or ASME SA-516 (Gr. 60 or Gr. 70) materials. Stainless steel [ASTM SA-240 (Type 304)] also has been used as liner material in some of the reinforced concrete containments.

2.4 Potential Degradation Mechanisms

Service-related degradation can affect the ability of the containment pressure boundary to perform satisfactorily in the unlikely event of a severe accident by reducing its structural capacity or jeopardizing its leaktight integrity. Degradation is considered to be any phenomenon that decreases the load-carrying capacity of a pressure-retaining component, limits its ability to contain a fluid medium, or reduces its service life. The root cause for component degradation can generally be linked to a design or construction problem, inappropriate material application, a base-metal flaw, or an excessively severe service condition. Component degradation can be classified as either material or physical damage. Determining whether material or physical damage has occurred often requires information about the service conditions to which the component was exposed and an understanding of the degradation mechanisms that could cause such damage. Information on the potential degradation mechanisms associated with these two damage classifications is provided in Ref. 17. Only a brief description of material and physical damage is provided below.

2.4.1 Material Damage

Material damage occurs when the microstructure of a metal is modified causing changes in its mechanical properties. When produced under controlled conditions, changes in the microstructure of a metal can have a beneficial effect (e.g., heat treating to produce a specified hardness). However, when the exposure conditions are not controlled, the mechanical properties (e.g., tensile and yield strength) of the affected metal can degrade to such an extent that the component is no longer suitable for its intended use. Degradation mechanisms that can potentially cause material damage to containment steels include (1) low-temperature exposure, (2) high-temperature exposure, (3) intergranular corrosion, (4) dealloying corrosion, (5) hydrogen embrittlement, and (6) neutron irradiation. Material damage to the containment pressure boundary from any of these sources is not considered likely, however.

2.4.2 Physical Damage

Physical damage occurs when the geometry of a component is altered by the formation of cracks, fissures, or voids, or its dimensions change due to overload, buckling, corrosion, erosion, or formation of other types of surface flaws. Changes in component geometry, such as wall thinning or pitting caused by corrosion, can affect structural capacity by reducing the net section available to resist applied loads. In addition, pits that completely penetrate the component can compromise the leaktight integrity of the component. Degradation mechanisms that potentially can cause physical damage to containment pressure boundary components include (1) general corrosion (atmospheric, aqueous, galvanic, stray-electrical current, general biological, molten-salt, liquid-metal, and high-temperature); (2) localized corrosion (filiform, crevice, pitting, and localized biological); (3) mechanically-assisted degradation (erosion, fretting, cavitation, corrosion fatigue, surface flaws, arc strikes, and overload conditions); (4) environmentally-induced cracking (stress-corrosion and hydrogen-induced); and (5) fatigue. Material degradation due to either general or pitting corrosion represent the greatest potential threat to the containment pressure boundary.

2.5 Requirements for Testing and Examination

Continued integrity of the containment pressure boundary is assessed through periodic testing and examinations.

2.5.1 Leakage-Rate Testing

Regulations for preservice and subsequent periodic containment leakage-rate testing are provided in Appendix J to 10 CFR 50.^{18,19} This regulation contains requirements pertaining to Type A, B, and C leakage-rate tests that must be performed by each licensee as a condition of their operating license. In September 1995, the NRC amended Appendix J (60 FR 49495) to provide a performance-based option for leakage-rate testing as an alternative to the existing prescriptive requirements. The amendment is aimed at improving the focus of the body of regulations by eliminating prescriptive requirements that are marginal to safety and by providing licensees greater flexibility for cost-effective implementation methods for regulatory safety objectives. Now that Appendix J has been amended, either Option A—*Prescriptive Requirements* or Option B—*Performance-Based Requirements* can be chosen by a licensee to meet the requirements of Appendix J.

Option A—Prescriptive Requirements

According to Option A requirements, Type A, B, and C leakage-rate tests must be performed at prescribed time intervals without regard for past operations or performance history.

Type A tests are designed to measure the overall leakage rate of the entire containment system. Three Type A tests must be conducted at approximately equal time intervals during each 10-year service period. The maximum allowable leakage rate (L_a) for the containment system is specified in the plant technical specifications, but the acceptable leakage rate for a Type A test may not exceed 0.75 L_a . A general inspection of accessible interior and exterior surfaces of the containment structure and components must be performed prior to each Type A test. The purpose of the inspections, which are usually based on visual observations, is to detect any evidence of structural deterioration that could adversely affect containment structural capacity or leaktight integrity. If structural deterioration is detected, corrective actions must be taken before the Type A test can be conducted.

Type B tests are conducted to detect local leaks and to measure leakage rates across penetrations with flexible metal seals, bellows expansion joints, airlock door seals, doors and penetrations with resilient seals or gaskets, and other components. Except for airlocks, these tests are conducted during the time the reactor is shutdown for refueling or at other convenient intervals, but in no case at intervals greater than two years. Airlocks must be tested at least

once every six months or at more frequent intervals depending on usage. The test pressure must not be less than the pressure associated with the design basis accident.

Type C tests measure isolation valve leakage rates. These tests are conducted each time the reactor is shutdown for refueling, but in no case at intervals greater than two years. The test pressure must not be less than the pressure associated with the design basis accident. Combined allowable leakage for all penetrations and valves subject to Type B and C tests is 0.60 La.

A summary technical report containing leakage rate test results must be prepared and submitted to the NRC for each periodic test. The report must include Type A, B, and C test results, an analysis and interpretation of the Type A test results, and a summary analysis of periodic Type B and C tests that were performed since the last Type A test. A separate summary report must also be provided if any Type A, B, or C test fails to meet the acceptance criteria.

Option B—Performance-Based Requirements

Now that Appendix J has been amended, licensees may voluntarily comply with Option B requirements rather than continue using established leakage-rate test schedules. Option B allows licensees with good integrated leakage-rate test performance histories to reduce the Type A testing frequency from three tests in 10 years to one test in 10 years. For Type B and C tests, Option B allows licensees to reduce testing frequency on a plant-specific basis based on the operating experience for each component and establishes controls to ensure continued performance during the extended testing interval. The NRC position on performance-based containment leakage-rate testing is discussed in Regulatory Guide 1.163.²⁰ Methods considered acceptable to the NRC staff for complying with the provisions of Option B are provided in guidance documentation prepared by the Nuclear Energy Institute (NEI).²¹

The NEI document presents an industry guideline for implementing the performance-based option and contains an approach that includes continued assurance of the leakage integrity of the containment without adversely affecting public health and safety, licensee flexibility to implement cost-effective testing methods, a framework to acknowledge good performance, and utilization of risk and performance-based methods. The guideline delineates the basis for a performance-based approach for determining Type A, B, and C containment leakage-rate surveillance testing frequencies using industry performance data, plant-specific performance data, and risk insights. It does not address how to perform the tests because these details can be found in existing documents.²² Licensees may elect to use other suitable methods or approaches to comply with Option B, but they must obtain NRC approval prior to implementation. However, a general inspection of accessible interior and exterior surfaces of the containment structure and components must be performed prior to each Type A test and during two other refueling outages before the next Type A test if the interval for the Type A test has been extended to 10 years.²⁰

2.5.2 In-Service Inspection

Preservice and in-service inspection requirements for metal containments and liners of concrete containments, and rules for containment pressure testing, are provided in Section XI, Division 1, Subsection IWE of the ASME Code.²³ This consensus standard addresses examination of accessible metal surfaces; seals, gaskets, and moisture barriers; dissimilar metal welds; and pressure-retaining bolting. Requirements for system pressure testing and criteria for establishing inspection programs and pressure-test schedules are contained in Appendix J.^{18,19} The inspections are intended to detect problems that could adversely affect the structural capacity of the containment and to periodically verify its leaktight integrity. Inspection requirements contained in Subsection IWE of the ASME Code (1992 Edition with 1992 Addenda) have been incorporated by reference into NRC regulations. This amendment became effective on September 9, 1996 and the utilities have five years to implement the examinations.

2.5.2.1 Containment Surfaces

Containment surface inspection requirements apply to metal containment pressure-retaining components and their integral attachments and to metallic shell and penetration liners of concrete containments. Areas requiring inspection include base metal and pressure-retaining weld surfaces that are accessible for either direct or remote visual examination. Surfaces that do not require in-service inspection include inaccessible portions of the containment and parts that are embedded in concrete. However, if there is degradation in an accessible region in close proximity to an inaccessible area, more detailed examination of this area is required. Inspection requirements for piping, pumps, and valves that complete the pressure boundary are provided elsewhere in Section XI.

Rules for containment surface inspection are intended to address the general inspection requirements specified in 10 CFR 50, Appendix J.^{18,19} and to provide requirements for periodic visual examinations of weld and base metal surfaces. According to the rules, containment surfaces must be inspected three times in a 10-year inspection interval. The inspections are performed during times when the plant is shutdown for refueling or maintenance. Rules pertaining specifically to general visual examinations; examinations of coated, non-coated, and weld surfaces; and containment surfaces requiring augmented examinations are provided in Subsection IWE and summarized below.

General Visual Examinations A general visual examination of all accessible containment weld and base metal surfaces (not including surface areas that are submerged or insulated) is required prior to each Type A leakage-rate test or during two other refueling outages before the next Type A test if the interval for the Type A test has been extended to 10 years.²⁰ The examination is performed either directly or remotely. Conditions considered suspect are required to be further evaluated, repaired, or replaced before the Type A leakage-rate test can be performed.

Coated, Non-Coated, and Weld Surface Examinations Detailed inspections of specific containment surface areas and pressure-retaining welds are required in conjunction with the general visual examination. These inspections are conducted by direct visual examination. In areas that are painted or coated, evidence of flaking, blistering, peeling, discoloration, and other signs of distress may be considered suspect and could require further evaluation, repair, or replacement. In areas that are not coated or painted, evidence of cracking, discoloration, wear, pitting, excessive corrosion, arc strikes, gouges, surface discontinuities, dents, and other signs of surface irregularities may also require further evaluation, repair, or replacement.

When surface flaws or suspect areas requiring further evaluation are detected, supplementary surface or volumetric nondestructive examinations may be required to determine the character of the flaw or to measure the extent of degradation. Magnetic particle and liquid penetrant are two surface examination techniques that could be used to establish the size, shape, and orientation of flaws. Radiographic, ultrasonic, and eddy current are three volumetric examination techniques that are commonly used to measure the extent of subsurface degradation.

Decisions to accept, repair, or replace defective areas are often based on comparisons between current nondestructive examination results and recorded results from preservice and prior in-service examinations. Areas that have experienced change but are considered acceptable can be placed back into service without repair or replacement. However, the nondestructive examination results must be recorded for use in future evaluations, and the defective areas must be periodically reexamined until the area remains essentially unchanged for three consecutive inspection periods.

Containment Surfaces Requiring Augmented Examinations Surface areas likely to experience accelerated degradation and aging require augmented examinations. These areas are specifically identified in the inspection program document prepared by the licensee and may include interior and exterior containment surfaces. Coated and uncoated areas requiring augmented examination are visually examined for evidence of coating degradation or surface flaws. Areas requiring further evaluation are then inspected using supplemental surface or volumetric examination techniques.

Examinations that reveal material loss exceeding 10 percent of the nominal containment wall thickness must be documented, evaluated, and then either accepted, repaired, or replaced. Flaws or degraded areas that are evaluated and considered nonstructural in nature or have no effect on the structural integrity of the containment can be considered acceptable for continued service without repair or replacement. However, areas that contain these flaws or degradation must be periodically reexamined until the area remains essentially unchanged for three consecutive inspection periods.

2.5.2.2 Seals, Gaskets, and Moisture Barriers

Containment seals, gaskets, and accessible surfaces of moisture barriers are required to be visually examined during each in-service inspection to detect wear, damage, erosion, tear, surface cracks, or other defects that could affect leaktight integrity. Seals and gaskets that are used to prevent leakage through airlocks, hatches, and other devices are required to be examined over their entire length. However, disassembly of sealed or gasketed connections is not required to merely provide access for inspection. Surfaces of containment moisture barriers that include flashing, caulking, and other sealants must be inspected if they are accessible. Moisture barriers (e.g., flashing, caulking, and other sealants), which may be located on the inside or outside of the containment and used at concrete-to-metal interfaces to prevent intrusion of moisture between the steel shell or liner and the concrete, must be inspected if they are accessible. Items considered defective must either be repaired or replaced.

2.5.2.3 Dissimilar Metal Welds

Surfaces of pressure-retaining dissimilar metal welds subject to cyclic loads and thermal stresses during normal plant operations are required to be visually examined during each in-service inspection. Dissimilar metal welds include those between carbon or low-alloy steels and high-alloy steels, carbon or low-alloy steels and high nickel alloys, and high-alloy steels and high nickel alloys. The examination area includes the weld metal and the base metal for 12.7 mm beyond the edge of the weld. Surface examinations are performed to detect planar flaws, but during any particular in-service inspection, only 50 percent of the dissimilar metal welds require inspection. Allowable flaw sizes are provided in Section XI, Division 1, Subsection IWB of the ASME Code.²⁴

2.5.2.4 Pressure-Retaining Bolting

Pressure-retaining bolted connections are required to be examined and tested during each in-service inspection. Surfaces of bolts, studs, nuts, bushings, washers, and threads in base metal and flange ligaments must be visually inspected for defects that could cause the connection to violate either leaktight or structural integrity requirements. However, disassembly of the connection is not required to provide access for inspection. Items considered defective must be replaced.

Bolt torque or tension testing is also required during the inspection, but only for bolted connections that were not disassembled and reassembled during the inspection interval. Either bolt torque or bolt tension are required to be within limits specified in the original design documents. When no limits have been specified, acceptable bolt torque or bolt tension limits must be established and used.

2.5.2.5 System Pressure Tests

Containment system pressure testing requirements also are provided in Subsection IWE and are essentially the same as those provided in Appendix J for Type A and Type B leakage-rate testing. Requirements for Type C pressure testing of isolation valves are not included because these components are not considered an integral part of the containment pressure vessel system.

2.6 Operating Experience

The carbon steel materials utilized to fabricate the steel containments and liners of reinforced concrete containments are susceptible to corrosion. As the nuclear plant containments age, degradation incidences are starting to occur at an increasing rate. Since 1986, there have been 32 reported occurrences of degradation associated with the containment pressure boundary at U.S. commercial nuclear power plants. In two cases, thickness measurements of the walls revealed areas that were below the minimum design thickness. There have been four cases where extensive corrosion of the liner has reduced the thickness locally by nearly one-half.²⁵ Table 1 presents a summary of documented instances of containment pressure boundary degradation.

Of the 32 instances of degradation since 1986, only four were detected through containment inspection programs conducted prior to Type A testing that were in effect at the time (i.e., preadoption by reference of basic requirements in ASME Subsection IWE). Nine of these occurrences were first identified by the NRC through its inspections or audits of plant structures. Examples of problems identified by the NRC were corrosion of the steel containment shell in the drywell sand cushion region, corrosion of the torus of the steel containment shell, and concrete containment liner corrosion. Eleven occurrences were detected by licensees while performing an unrelated activity, or after they were alerted to a degraded condition at another site.

3. INSPECTION OF AGED/DEGRADED CONTAINMENTS PROGRAM

The Inspection of Aged/Degraded Containments Program has several objectives that will be accomplished prior to the program's completion. Corrosion mechanisms and susceptible pressure boundary material types and locations will be identified and discussed. An assessment of available destructive and nondestructive techniques for examining the containment pressure boundary will be conducted. Effectiveness and limitations of methods available to prevent or mitigate corrosion, and root-cause resolution practices to maintain or reestablish structural capacity margins and leaktight integrity will be established. Techniques for assessing the presence and rate of occurrence of corrosion, particularly in inaccessible regions of steel containments and liners, will be identified and evaluated. Criteria will be established for evaluating weld and base metal integrity, penetration and bellows integrity and leak tightness, potential for intergranular stress corrosion cracking and fatigue cracking in metal containments, and flaw and crack sizes. A quantitative structural reliability analysis methodology will be developed for application to degraded steel containments and liners of reinforced concrete containments. Methods will be developed and implemented for estimating containment degradation rates and remaining service lives. Factored into development of the methodology will be an assessment of probabilistic risk assessment models used in previous risk assessments, and time-dependent models developed to investigate the structural reliability under both design basis and severe accident loading conditions of steel containments and liners experiencing various degrees of degradation. Fragility curves will be utilized to model the probability of structural failure under various scenarios. The methods developed for assessing the probability that containment capacity has degraded below a specified level will incorporate uncertainties in material properties, inspection/sampling results, as well as future demands imposed by operating conditions. In meeting these objectives, activities are conducted under three task areas: (1) program management, (2) steel containment/liner assessment and root-cause resolution practices, and (3) reliability-based condition assessment.

3.1 Program Management

The overall objective of the program management task is to effectively manage the technical tasks undertaken to understand the significant factors relating to occurrence of corrosion, efficacy of inspection, and structural capacity margins of steel containments and liners. Primary activities have included program planning and resource allocation, program monitoring and control, and documentation and technology transfer. Under the first of these activities a subcontract has been implemented to meet objectives of the third task area. The program monitoring and control

Table 1 Instances of containment pressure boundary component degradation at commercial nuclear power plants in the United States.

Plant Designation (Occurrence Date) Plant Type (Source)*	Containment Description (No. of Similar Plants)	Degradation Description	Detection Method
Vermont Yankee (1978) BWR (Ref. 2.1)	Mark I Steel drywell and wetwell (22)	Surface cracks in the overlay weld-to-torus base metal heat- affected zone	Visual examination (As part of modifications to restore the originally intended design safety margins)
Hatch 2 (1984) BWR (Refs. 2.2, 2.3, and 2.6)	Mark I Steel drywell and wetwell (22)	Through-wall cracks around containment vent headers within the containment torus (Brittle fracture caused by injection of cold nitrogen into torus during inerting)	Visual examination of torus interior
Hatch 1 (1985) BWR (Ref. 2.6)	Mark I Steel drywell and wetwell (22)	Through-wall crack in nitrogen inerting and purge line (Brittle fracture caused by injection of cold nitrogen during inerting)	In-service inspection testing using magnetic particle method
Monticello (1986) BWR (Ref. 2.56)	Mark I Steel drywell and wetwell (22)	Polysulfide seal at the concrete- to-shell interface became brittle allowing moisture to reach the steel shell	Visual examination (A small portion of the drywell shell was excavated as a part of a life extension study)
Dresden 3 (1986) BWR (Ref. 2.7)	Mark I Steel drywell and wetwell (22)	Coating degradation due to exposure to fire with peak metal temperatures of 260°C (500°F) and general corrosion of metal shell by water used to extinguish fire	Visual examination (Polyurethane between the drywell shell and concrete shield wall was ignited by arc-air cutting activities producing smoke and heat)
Oyster Creek (1986) BWR (Refs. 2.8, 2.22, and 2.35)	Mark I Steel drywell and wetwell (22)	Defective gasket at the refueling pool allowed water to eventually reach the sand cushion region causing drywell shell corrosion	Visual examination of uncoated areas and ultrasonic inspection
Fitzpatrick (1987) BWR (Refs. 2.56 and 2.29)	Mark I Steel drywell and wetwell (22)	Degradation of torus coating with associated pitting	Visual examination of uncoated areas and ultrasonic inspection (Technical specification surveillance performed during outage)
Millstone 1 (1987) BWR (Ref. 2.29)	Mark I Steel drywell and wetwell (22)	Degradation of torus coating	Visual examination of uncoated areas and ultrasonic inspection (The torus had been drained for modifications)
Oyster Creek (1987) BWR (Ref. 2.29)	Mark I Steel drywell and wetwell (22)	Degradation of torus coating with associated pitting	Visual examination of uncoated areas and ultrasonic inspection

* Reference numbers provided in this table correspond to references listed in Chapter 2 of Ref. 17.

Table 1 (cont.) Instances of containment pressure boundary component degradation at commercial nuclear power plants in the United States.

Plant Designation (Occurrence Date) Plant Type (Source)*	Containment Description (No. of Similar Plants)	Degradation Description	Detection Method
Brunswick 1 (1987) BWR (Ref. 2.18)	Mark I Reinforced concrete with steel liner (9)	Corrosion of steel liner	General visual examination of coated areas
Nine Mile Point 1 (1988) BWR (Ref. 2.9)	Mark I Steel drywell and wetwell (22)	Corrosion of uncoated torus surfaces	Visual examination of uncoated areas and ultrasonic inspection
Pilgrim (1988) BWR (Ref. 2.29)	Mark I Steel drywell and wetwell (22)	Degradation of torus coating	Visual examination of uncoated areas and ultrasonic inspection (Licensee inspection as a result of occurrences at similar plants)
Brunswick 2 (1988) BWR (Ref. 2.18)	Mark I Reinforced concrete with steel liner (9)	Corrosion of steel liner	General visual examination of coated areas
Dresden 2 (1988) BWR (Ref. 2.23)	Mark I Steel drywell and wetwell (22)	Coating, electrical cable, and valve operator component degradation due to excessive operating temperatures	Visual examination of uncoated areas and ultrasonic inspection (Ventilation hatches in the drywell refueling bulkhead inadvertently left closed)
Hatch 1 and 2 (1989) BWR (Ref. 2.11)	Mark I Steel drywell and wetwell (22)	Bent anchor bolts in torus supports (due to weld induced radial shrinkage)	Visual examination
McGuire 2 (1989) PWR (Ref. 2.15)	Ice Condenser Steel cylinder (16)	Corrosion on outside of steel cylinder in the annular region at the intersection with the concrete floor	General visual examination prior to Type A leakage rate test
McGuire 1 (1989) PWR (Ref. 2.15)	Ice Condenser Steel cylinder (16)	Corrosion on outside of steel cylinder in the annular region at the intersection with the concrete floor	General visual examination (Inspection initiated as a result of corrosion detected at McGuire 2)
Catawba 1 (1989) PWR (Refs. 2.15 and 2.25)	Ice Condenser Steel cylinder (16)	Corrosion on outside of steel cylinder in the annular region	General visual examination (Inspection initiated as a result of corrosion detected at McGuire 2)
Catawba 2 (1989) PWR (Ref. 2.15)	Ice Condenser Steel cylinder (16)	Corrosion on outside of steel cylinder in the annular region	General visual examination (Inspection initiated as a result of corrosion detected at McGuire 2)

* Reference numbers provided in this table correspond to references listed in Chapter 2 of Ref. 17.

Table 1 (cont.) Instances of containment pressure boundary component degradation at commercial nuclear power plants in the United States.

Plant Designation (Occurrence Date) Plant Type (Source)*	Containment Description (No. of Similar Plants)	Degradation Description	Detection Method
McGuire 1 (1990) PWR (Ref. 2.24)	Ice Condenser Steel cylinder (16)	Corrosion on outside of steel cylinder in the annular region	General visual examination (Follow-up inspection by licensee)
McGuire 1 (1990) PWR (Ref. 2.16, 2.24, and 2.26)	Ice Condenser Steel cylinder (16)	Corrosion on inside surface of coated containment shell under the ice condenser and between the floors near the cork filler material	Visual examination and ultrasonic inspection (Degradation possibly caused by moisture from the ice condenser or condensation)
Quad Cities 1 (1991) BWR (Refs. 2.30, 2.31, and 2.17)	Mark I Steel drywell and wetwell (22)	Two-ply containment penetration bellows leaked due to transgranular stress-corrosion cracking	General visual examination (Excessive leakage detected)
Quad Cities 2 (1991) BWR (Refs. 2.30 and 2.31)	Mark I Steel drywell and wetwell (22)	Two-ply containment penetration bellows leaked due to transgranular stress-corrosion cracking	General visual examination (Excessive leakage detected)
Dresden 3 (1991) BWR (Ref. 2.31)	Mark I Steel drywell and wetwell (22)	Two-ply containment penetration bellows leaked due to transgranular stress-corrosion cracking	General visual examination (Excessive leakage detected)
Point Beach 2 (1992) PWR (Ref. 2.27)	Post-tensioned concrete cylinder with steel liner (41)	Liner plate separated from concrete	General visual examination
H. B. Robinson (1992) PWR (Ref. 2.27)	Post-tensioned concrete cylinder with steel liner (41)	Degradation of liner coating	General visual examination
Cooper (1992) BWR (Ref. 2.27)	Mark I Steel drywell and wetwell (22)	Corrosion of interior torus surfaces and corrosion stains on exterior torus surface in one area	General visual examination
Beaver Valley 1 (1992) PWR (Refs. 2.27 and 2.33)	Subatmospheric Reinforced concrete cylinder with steel liner (7)	Corrosion of steel liner, degradation of liner coating, and instances of liner bulging	General visual examination prior to Type A leakage rate test

* Reference numbers provided in this table correspond to references listed in Chapter 2 of Ref. 17.

Table 1 (cont.) Instances of containment pressure boundary component degradation at commercial nuclear power plants in the United States.

Plant Designation (Occurrence Date) Plant Type (Source)*	Containment Description (No. of Similar Plants)	Degradation Description	Detection Method
Salem 2 (1993) PWR (Ref. 2.32)	Reinforced concrete cylinder with steel liner (11)	Corrosion of steel liner	General visual examination prior to Type A leakage rate test
Sequoyah 1 (1993) PWR (Ref. 2.20)	Ice Condenser Steel cylinder with concrete shield building (8)	Degradation of moisture barriers resulting in corrosion of the steel shell	General visual examination and visual examination of coated areas
Sequoyah 2 (1993) PWR (Ref. 2.20)	Ice Condenser Steel cylinder with concrete shield building (8)	Degradation of moisture barriers resulting in corrosion of the steel shell	General visual examination and visual examination of coated areas
Brunswick 2 (1993) BWR (Refs. 2.18 and 2.19)	Mark I Reinforced concrete with steel liner (9)	Corrosion of steel liner	General visual examination and visual examination of coated areas (Follow-up inspection based on conditions noted in 1988)
Brunswick 1 (1993) BWR (Ref. 2.19)	Mark I Reinforced concrete with steel liner (9)	Corrosion of steel liner	General visual examination and visual examination of coated areas (Inspection initiated as a result of corrosion detected at Brunswick 2)
McGuire 1 (1993) PWR (Ref. 2.34)	Ice Condenser Steel cylinder (16)	Main steam isolation line bellows leakage	Leakage testing conducted on bellows following successful Type A leakage rate test
Braidwood 1 (1994) PWR (Ref. 2.21)	Post-tensioned concrete cylinder with steel liner (41)	Liner leakage detected but not located	Type A leakage rate test

* Reference numbers provided in this table correspond to references listed in Chapter 2 of Ref. 17

activity has primarily addressed the preparation of management and financial reports, and participation in NRC information and review meetings. Documentation and technology transfer activities include program coordination with other government agencies-related activities, participation in technology working groups such as ASME Section XI, coordination with foreign technologies, and technology exchange through participation in national and international activities such as participation in the International Atomic Energy Agency Coordinated Research Program on Concrete Containment Buildings.

3.2 Steel Containment/Liner Assessment and Root-Cause Resolution Practices

The overall objectives of this task are to (1) identify procedures to quantitatively assess the presence, magnitude, and significance of any degradation factors that could impact structural capacity margins; and (2) provide data for use in current and future structural condition assessments. In addition, techniques will be established for (1) characterization of steel containment, steel liner, coating, and sealant materials; (2) mitigation of environmental stressor or aging factor effects; and (3) root-cause resolution evaluations. Current activities include characterization of containment pressure boundary materials, development of a degradation assessment methodology, and evaluation of destructive and nondestructive evaluation techniques and methodologies.

3.2.1 Characterization of Containment Pressure Boundary Materials

Characterization of containment pressure boundary materials addressed the collection and presentation of data and information on these materials, and quantifying the affects (if any) of degradation factors such as corrosion on their properties. Desired data and information requirements for characterization of containment pressure boundary materials have been developed (i.e., general information covering a description of the material, processing information, and baseline data; material composition in terms of chemistry; and mechanical, physical, and other properties). Structural steels acceptable for use in the construction of the containment pressure boundary have been identified along with their corresponding American Society of Mechanical Engineers (ASME) and American Society for Testing and Materials specifications (ASTM) (e.g., carbon steel, ASME SA-36, and ASTM A 36). Potential nuclear power plant-related degradation factors for these materials have been identified, with corrosion being the most important. Two options were identified for presentation of the materials property data and information —incorporate additional chapters into the existing Structural Materials Information Center,²⁶ or use of object-oriented relational data base software to develop a customized data base. Conclusions and recommendations to guide the collection and incorporation of data and information into a database on containment pressure boundary materials are provided in Ref. 16.

3.2.2 Degradation Assessment Methodology

A degradation assessment methodology intended for use in characterizing the in-service condition of metal and concrete containment pressure boundary components and quantifying the amount of damage present has been developed.¹⁷ Because information required to characterize and quantify the condition of degraded components must be established on a case-by-case basis taking into consideration unique containment design features and plant operating constraints, the methodology does not include a step-by-step procedure. It has been developed in the form of general, non-prescriptive guidance.

Condition assessments are an essential element of both continued service evaluations and informed aging-management decisions. From an aging management perspective, metal and concrete pressure boundary components that exhibit satisfactory long-term performance and do not experience in-service degradation can be considered acceptable for continued service. However, components found by in-service testing or examination to be deteriorated or damaged must be evaluated to determine whether continued service is appropriate or whether repairs, replacements, or retrofits are needed. Damage is considered significant when it adversely affects structural capacity, leaktight integrity, or remaining service life. Requirements for corrective actions to be taken when evidence of structural deterioration is discovered are provided in Ref. 18. More detailed acceptance standards and evaluation criteria for use in determining the acceptability of degraded components for continue service are provided in Section XI, Division 1, Subsection IWE of the ASME Code.²³ A diagram that illustrates the continued service evaluation process presented in Subsection IWE is shown in Fig. 1.

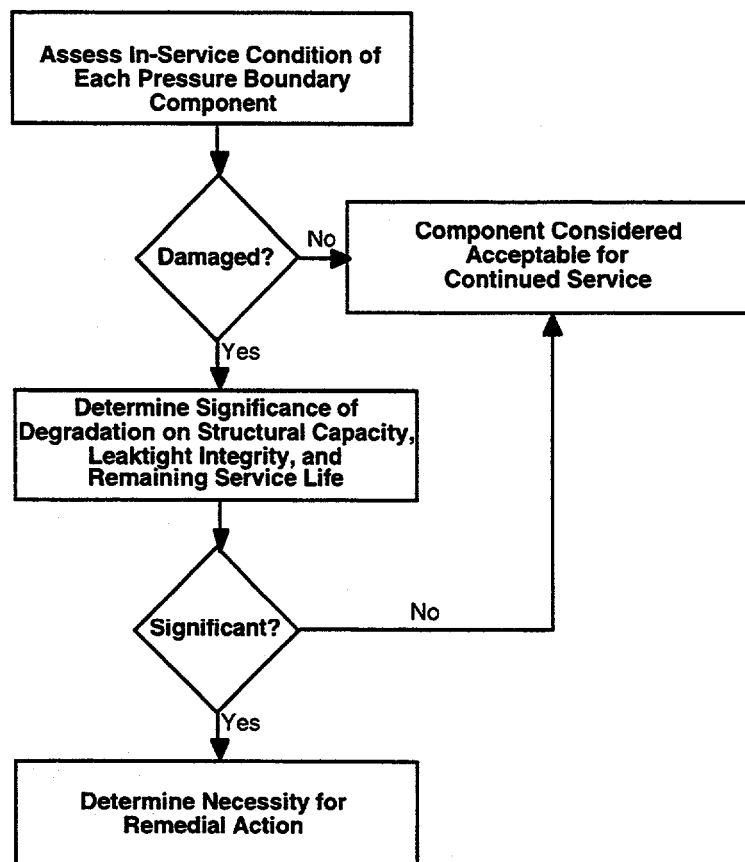


Fig. 1 Continued service evaluation process for containment pressure boundary components.

Continued service evaluations are performed by qualified engineers and authorized personnel who determine the adequacy of components for their intended use.²⁷ The decision-making process begins with an understanding of the in-service condition of each containment component. Condition assessments that provide essential information for continued service evaluations involve detecting damage, classifying the types of damage that may be present, determining the root cause of the problem, and quantifying the extent of degradation that may have occurred. Knowledge gained from condition assessments can serve as a baseline for evaluating the safety significance of any damage that may be present and defining in-service inspection programs and maintenance strategies. Condition assessment results can also be used to estimate future performance and remaining service life. Four primary topics are associated with in-service condition assessments for metal and concrete containment pressure boundary components – damage detection, damage classification, root-cause determination, and damage measurement. Additional details on each of these topics and their interaction are provided in Ref. 17.

3.2.2.1 Damage Detection

Damage detection is the first and most important step in the condition assessment process. Routine observation, general visual inspections, leakage-rate testing, and nondestructive examinations are techniques frequently used to identify areas of the containment that have experienced degradation. However, damage such as wall thinning caused by corrosion can occur in inaccessible locations making detection difficult or impossible. Knowing where to inspect and what type of damage to anticipate often requires information about the design features of the containment and the materials used to construct its pressure-retaining components.

3.2.2.2 Damage Classification

Damage occurs when the microstructure of a material is modified by exposure to a severe environment or when the geometry of a component is altered. Determining whether material or physical damage has occurred often requires information about the service conditions to which the component was exposed and an understanding of the degradation mechanisms that could cause such damage.

3.2.2.3 Root-Cause Determination

The root cause for component degradation can generally be linked to a design or construction problem, inappropriate material application, a base-metal flaw, or an excessively severe service condition. Determining what caused the degradation helps identify the type of damage that has occurred and define appropriate actions to be taken to reduce or eliminate further deterioration.

3.2.2.4 Damage Measurement

One way to evaluate the significance of containment pressure boundary component degradation on structural capacity and leaktight integrity is by comparing its preservice condition to its condition after degradation has occurred. Condition assessment accuracy depends on the availability of quantifiable evidence such as dimensions of corroded surface areas, depths of corrosion penetration, or changes in material properties that indicate the extent and magnitude of the degradation. Methods for quantifying component degradation involve either nondestructive examination or destructive testing. Results from these investigations provide a measure of the extent of degradation at the time the component was examined. Techniques for establishing time-dependent change such as corrosion and wear rates involve periodic examination or testing. In-service monitoring provides a way to measure time-dependent changes in component geometry or material properties and to detect undesirable changes in operating conditions that could affect useful service life.

3.2.3 Destructive and Nondestructive Evaluation Techniques

Although the performance of the containment pressure boundary has been good, as the plants age, degradation incidences (e.g., corrosion) are starting to emerge (e.g., Section 2.6). If undetected, the possibility exists that the degradation effects may reduce the margin that the containments have to accommodate accidents. An essential element in the assessment of the integrity (or in the determination of available safety margins) of a containment structure is knowledge of the damage state of its materials of construction. Future condition assessments require not only knowledge of the current damage state, but knowledge of the change in damage state with time. In-service inspections and testing are performed to measure the current state of damage. Changes in damage state with time can be estimated through approaches such as physical models, correlation relations, or trending analyses. Many of the existing in-service inspection techniques have been developed primarily for the detection and assessment of fabrication-related flaws under controlled conditions. These techniques may not be adequate for use in helping to effectively manage the aging of the containment pressure boundary in NPPs. In addition, accessibility of the containment pressure boundary may be restricted due to the presence of coatings, its location below water level or embedded in concrete, or certain areas may be accessible only from one surface. Because of the safety significance of the containment pressure boundary, the in-service inspections generally require a higher level of reliability and more quantitative definition of defects present than those associated with the general manufacturing sector. Basic approaches used to quantify the extent of damage present, or its change with time, include nondestructive examination, destructive testing, and in-service monitoring.

3.2.3.1 Nondestructive Examination

Nondestructive examination (NDE) is the development and application of technical methods to examine materials or components in ways that do not impair their future usefulness and serviceability in order to detect,

locate, measure, and evaluate discontinuities, defects, and other imperfections; to assess integrity, properties, and composition; and to measure geometrical characteristics.²⁸ From an operational viewpoint, such examinations are required to identify potential challenges to structural or leaktight integrity in time to take remedial action. They also play an important role in structural reliability assessment, especially when combined with failure analysis techniques such as fracture mechanics. Nondestructive examination provides an opportunity to revise and update the probability models used to determine current margins of safety and to forecast future reliability and performance (e.g., Section 3.3). Factors that are important in a condition assessment include probability of detection, threshold of detection and flaw size distribution, and sizing accuracy.

The most common NDE techniques in civil structures are visual inspection, liquid penetrant, magnetic particle, ultrasonic, eddy current, and radiography. Visual inspection is the oldest and still most widely used NDE method. Visual inspection can identify regions of corrosion, or peeling or blistering of coatings that may indicate damage to the substrate. Liquid penetrant is effective in locating surface flaws in essentially nonporous materials. The fluorescent or visible penetrant seeps into various types of minute surface openings by capillary action, giving indications of defects. The advantage of this method is that it depends neither on ferromagnetism (as does, for example, magnetic particle inspection) nor on defect orientation as long as only surficial flaws are considered. The major limitation of liquid penetrant inspection is that it cannot detect subsurface flaws and can be excessively influenced by the surface roughness or porosity. Magnetic particle inspection is utilized to reveal surface and subsurface discontinuities in ferromagnetic materials. When the material is magnetized, a leakage field is generated by magnetic discontinuities that lie in a direction transverse to the direction of the magnetic field. The leakage field gathers and holds some of the fine ferromagnetic particles that are applied over the material surface. This forms an outline of the discontinuity and indicates its location, size, and shape. Magnetic particle inspection is capable of detecting fine, sharp and shallow surface cracks, but is not good for wide and deep defects. It cannot be used for nonferromagnetic materials. The magnetic field must be in a direction that intercepts the principal plane of discontinuity for a good result. Thin coatings of paint and other nonmagnetic coverings will adversely affect the sensitivity. Ultrasonic inspection is used to detect both surface and internal discontinuities in materials and can also be used to identify areas of thinning due to corrosion. Beams of high frequency sound waves introduced into the material attenuate due to wave scattering and are partially or completely reflected at interfaces. The reflected beam is displayed and analyzed to define the presence and location of defects such as cracks or voids. Ultrasonic inspection also can be used to measure thickness and extent of corrosion by monitoring the transit time of a sound wave through the component, or the attenuation of its energy. Ultrasonic inspection can be performed under water. Its principal advantages are its portability, and superior penetrating power and volumetric scanning ability which allow the detection of deep flaws. Its disadvantage is that defects in parts that have rough or irregular surfaces, or are very small, thin or nonhomogeneous are difficult to detect. Eddy current is effective in detecting defects at or within a few millimeters of the surface. It is based on the principle of electromagnetic induction. Induced current flow in the test article is impeded and its direction changed (i.e., electromagnetic field is altered) by the presence of a flaw or discontinuity. Thus surface discontinuities having a combination of predominantly longitudinal and radial dimensional components can readily be detected. A majority of surface discontinuities can be detected by eddy current with high speed and low cost. If a coating is present, it need not be removed. However, the sensitivity of eddy current to defects beneath the surface is decreased. Also, laminar defects may not alter the current flow enough to be detected. Radiography methods are based on the differences in absorption by different portions of a component of penetrating radiation, such as X-ray or γ -ray. The images produced can be analyzed to locate flaws. Planar defects cannot be detected unless their principal plane is essentially parallel to the radiation beam. Tight cracks are difficult to detect regardless of orientation. In contrast to the other methods, radiography requires access to both sides of the component. Safety protocols also must be followed and radiography is relatively expensive.

None of the NDE techniques noted above are perfect. Results obtained depend on many factors, including the sensitivity of the instruments to different types of flaws; human factors such as education, training and proficiency of operators; geometry and microstructure of the component inspected; and size of flaws. Many of the NDE methods may be difficult to use in condition assessment and aging management, where quantification of flaw

size is necessary and limitations on the sensitivity of NDE are amplified by difficult field conditions. The procedures used in service frequently are manual and time consuming. A flaw of a given size can be detected only with a certain probability; for any but the largest defects, however, there is a finite probability that the flaw escapes detection. Conversely, there is a possibility that NDE indicates a flaw when none is present (a so called false call); remedial actions in such a case not only would be unnecessary but might damage the structure. Moreover, the actual flaw present may not be measured accurately by the NDE method chosen.

3.2.3.2 Destructive Testing

Tests that alter the shape, form, size, or structure of the material being tested are considered destructive tests. These tests may be performed to determine mechanical, physical, chemical, thermal, or other properties of the material, or to examine the material for microstructural imperfections, voids, or inclusions. Destructive tests are commonly used to determine mechanical properties of metallic materials and can involve tension, compression, ductility, shear, torsion, bend, creep, stress-relaxation, hardness, fatigue, or fracture testing. These tests are usually conducted in room-temperature air, but they can also be performed at higher or lower temperatures or under other environmental conditions. Test methods that require the removal and testing of representative portions of material from a component are also considered destructive tests when the affected component is rendered useless or unfit for future service. As part of a damage assessment process, tension, hardness, and metallographic testing may be conducted on material samples removed from containment pressure boundary components. Measurements obtained during tension testing can be used to develop stress-strain curves and to establish mechanical property values such as the modulus of elasticity, ultimate tensile elongation, ultimate tensile strength, yield strength, and reduction of area. Property values obtained from tension testing are generally used to determine conformance or nonconformance with material specifications. However, test results can also be used to compare the performance and properties of replicate specimens tested under a variety of exposure conditions or using different testing methods. Replicate specimen results can provide a basis for establishing limits on environmental exposure, working stresses, or operating temperatures. Hardness testing (e.g., Brinell or Rockwell) that uses small diamond points or hardened round steel balls to produce permanent indentations or deformations in the surface of the material being tested, is widely used for determining the relative quality of a metallic component and to establish the uniformity of its material properties. It is relatively easy to perform, requires very little material or surface preparation, and usually causes minimal surface damage to the material or component. Metallography is the branch of science that relates to the constitution and structure, and their relation to the properties of metals and alloys. Testing is usually performed in a laboratory set up where metallic specimens are prepared for microscopic examination. These examinations are conducted to reveal the constituents and structure of the material. Metallography is probably the most useful destructive testing method available for identifying differences in material microstructure caused by exposure to high temperatures or severe environments.

3.2.3.3 In-Service Monitoring

In-service monitoring involves examination of a component while it remains in service. It is generally used for repeated examinations of a flawed component or suspect area to monitor change with time. Data collection can be performed on a case-by-case basis at irregular intervals or at prescribed times using a computer-controlled data acquisition system. Results from in-service monitoring can provide valuable information for assessing the current condition of a degraded component, estimating its remaining useful service life, and making informed aging-management decisions. An example of in-service monitoring would be on-line electrochemical measurements to establish the average degradation rate of a component caused by corrosion, or the cumulative metal loss or the instantaneous corrosion rate of a component under actual service conditions.

3.3 Reliability-Based Condition Assessments

The overall objectives of this task are to (1) identify mathematical models from principles of structural mechanics to evaluate degradation in strength of containment pressure boundaries structures over time,

(2) recommend statistically-based sampling plans for inspection of steel structures to ensure that any damage present will be detected with a specified level of confidence, and (3) develop reliability-based methods to assess the probability that steel containment capacity has degraded below a specified level. This task will provide quantitative evidence that the strength of the containment pressure boundary is sufficient to withstand operating and environmental events with a level of reliability that is sufficient to protect public health and safety.

Structural aging may cause the integrity of the containment pressure boundary to evolve over time. Its strength and stiffness properties may degrade in hostile service environments from corrosion, fatigue or crack propagation, or material changes. An evaluation of the reliability of the containment pressure boundary during a period of continued service must include these past challenges on its integrity. The random fashion in which degradation occurs must be taken into account in development of risk management policies and procedures. Uncertainties that complicate the evaluation of aging effects in structures arise from a number of sources: (1) inherent randomness in structural loads; (2) lack of in-service measurements and records; (3) limitations in available models for quantifying time-dependent material changes and their contribution to containment pressure boundary integrity; (4) inadequacies of nondestructive evaluation techniques; and (5) shortcomings in existing methods for remedial actions. Activities are addressing development of an approach for condition assessment and damage analysis to relate significant material aging factors and degradation and structural loads to engineering properties needed for a structural assessment. Activities to date have been conducted under three main subtasks: (1) damage assessment and life prediction model development, (2) data assemblage, and (3) methodology development and application. A fourth subtask entitled risk assessment and fragility modeling of containment pressure boundaries will be implemented in 1997. Additional information to that provided below is available in Refs. 29 and 30.

3.3.1 Damage Assessment and Life Prediction Model Development

Predictive models based on principles of structural mechanics, supported by limited experimental data, enable the change in strength with time of a steel structure to be evaluated in terms of initial conditions, applied load history, and a parameterization of an aggressive environment. For some mechanisms of strength degradation (e.g., stable crack growth under cyclic load), the mechanics of deterioration are reasonably well established and predictable through fracture mechanics.³¹⁻³³ In other cases (e.g., thermal cycling and irradiation), the behavioral models are less certain requiring development from laboratory tests. These mechanisms, however, are considered to be of secondary importance relative to corrosion or fatigue.

Corrosion and fatigue were identified as the most significant mechanisms of deterioration for the containment pressure boundary. Accurate models of their structural effects are required to predict degradation in strength and stiffness. A thorough review of the literature on corrosion revealed only simple empirical models for general and pitting corrosion growth; statistical treatments of corrosion are practically nonexistent. Fatigue also is treated empirically, but more statistical data are available.

The empirical nature of life prediction models identified and the lack of visible or detectable damage during initiation and early growth stages led to the consideration of recent advances in continuum damage mechanics³⁴ as a basis for condition assessment procedures. Damage accumulation models were developed from first principles of thermodynamics for mechanisms of gross inelastic deformation, (elevated temperature) creep, and low-cycle fatigue. Validation of these models with limited experimental data on aircraft-grade steel and aluminum alloys is excellent; however, similar data for validation purposes on ferritic steels are less readily available, but are gradually being collected.

3.3.2 Data Assemblage

Structural aging and deterioration cause the conditional failure rate of a structure to increase with time. Data to describe the factors that can impact structural durability are being quantified (i.e., operating conditions,

environmental stressors, design bases, material quality, and construction methods). Uncertainties associated with those data are being determined. Structural loads arising from service, extreme environmental, or accidental conditions are being modeled as stochastic processes, as well as environmental conditions under which the structures operate. In the development of condition assessment methodologies for concrete structures,³⁵ it was found that the loads could be modeled as Poisson pulse processes.^{36,37} It is believed that a continuous rather than a discrete stochastic representation of load may be necessary to evaluate time-dependent performance of steel containments.

Statistical data are necessary to support the predictive damage models. Data to describe the growth of corrosion were collected for different environments. Factors that impact structural durability were identified. Stochastic process models of structural loads arising from service, extreme environmental and accident conditions were identified. Quantification of uncertainties associated with structural loadings has been initiated. An assessment of nondestructive evaluation methods was performed, with particular attention given to determining probabilities of detecting defects of various sizes and quantifying defect measurement uncertainties. This assessment has yet to be completed.

3.3.3 Methodology Development and Application

The reliability-based condition assessment methodologies will quantify residual structural strength or predict future service life. They will integrate areas of damage mechanics, stochastic characterization of the plant environment, service load history, and current strength to determine probability distributions of structural safety margins at some future time. Probability distributions of additional usable life associated with a minimum structural capacity will be determined. The time-dependent reliability analysis can be used to determine intervals of inspection and maintenance. The main effect of inspection/repair is to remove some of the larger defects from the structure, thereby upgrading its strength and shifting its conditional failure rate downward. Incorporation of nondestructive examination information into a reliability-based structural condition assessment can have significant long-term economic and safety benefits. Statistically-based sampling plans will be developed to guide the extent of the structure to be inspected during each scheduled inspection/maintenance period. Bayesian statistical methods will be used in the development of tools to determine the impact of alternate in-service inspection and maintenance strategies on performance and risk management. Finally, a cost-benefit analysis of alternate inspection/maintenance strategies incorporating estimates of structural performance and reliability will be used to develop rational in-service inspection/maintenance policies to support service life extension.

An assessment of time-dependent reliability analysis techniques led to the development of models that are suitable for the current project. Two sensitivity studies were conducted to demonstrate the feasibility of these models when applied to uniform corrosion, one of an internally pressurized cylindrical shell of revolution and the second of a flexural member. The analysis was performed using a conditional expectation form of Monte Carlo simulation. Preliminary results indicate that the random corrosion incubation time is more significant than the corrosion rate parameter for steel plates of typical thickness in NPPs (i.e., 25 to 35 mm). As was observed earlier with NPP concrete structures,³⁵ the conditional failure rate of an aging steel structure increases nonlinearly in time.

4.0 APPLICATION OF RESULTS

Potential regulatory applications of this research include: (1) improved predictions of long-term material and structural performance and available safety margins at future times; (2) establishment of limits on exposure to environmental stressors; (3) reduction in total reliance by licensing on inspection and surveillance through development of a methodology that will enable the integrity of structures to be assessed (either pre- or post-accident); and (4) improvements in damage inspection methodology through potential incorporation of results into national standards which could be referenced by standard review plans.

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ANALYSES OF CONTAINMENT STRUCTURES WITH CORROSION DAMAGE¹

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Abstract: Corrosion damage that has been found in a number of nuclear power plant containment structures can degrade the pressure capacity of the vessel. This has prompted concerns regarding the capacity of corroded containments to withstand accident loadings. To address these concerns, finite element analyses have been performed for a typical PWR Ice Condenser containment structure. Using ABAQUS, the pressure capacity was calculated for a typical vessel with no corrosion damage. Multiple analyses were then performed with the location of the corrosion and the amount of corrosion varied in each analysis. Using a strain-based failure criterion, a "lower bound", "best estimate", and "upper bound" failure level was predicted for each case. These limits were established by: determining the amount of variability that exists in material properties of typical containments, estimating the amount of uncertainty associated with the level of modeling detail and modeling assumptions, and estimating the effect of corrosion on the material properties.

INTRODUCTION

Corrosion damage has been found in a number of existing containments (Shah et al., 1994). Tests conducted by Sandia National Laboratories on scaled containment models and analytical efforts have demonstrated that containments without corrosion can resist static internal pressures that are well above the containment's design pressure without significant leakage (Jung, 1984; Koenig, 1986; Reese and Horschel, 1985; Miller, 1990; Fanous et al., 1993). However, corrosion found in containments of operating nuclear power plants has prompted concerns regarding the capacity of the corroded containment to withstand accident loadings.

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Corrosion of containments may reduce the margin between the design and failure pressure and thus might increase the probability of containment failure under severe accident loadings. Comparisons of the shell thickness after corrosion with the ASME (ASME, 1992) Code minimum allowable thickness gives some indication of the reduction in containment capacity. However, the nature and the location of the corrosion must be considered to determine how much the affected areas degrade the ultimate capacity of the containment. For example, considerable corrosion in an area with excess thickness may not affect the ultimate capacity, while minor corrosion in a critical area would lower the ultimate capacity.

The first part of this paper reviews the basis for using finite element methods to predict the ultimate capacity of corroded pressure vessels. This section describes: research and analyses that have been done for the petroleum industry to understand the capacity of corroded pipelines, material properties of corroded steel, finite element modeling options, and strain-based failure criteria for corroded steel structures. In the second part of this paper, multiple analyses were performed for a typical PWR Ice Condenser containment, with the amount of corrosion and the location of corrosion varied in each analysis. Corrosion was modeled in the steel shell near the: concrete basemat, upper floor location, and ice basket region. At each of the three corroded areas, two levels of degradation were analyzed. The first level corresponded to the amount of corrosion damage necessary to cause failure to initiate at the corroded location. The second level of damage was selected by increasing the damage to the area so that the pressure capacity of the structure was further reduced.

BASIS FOR PREDICTING CAPACITY OF CORRODED CONTAINMENTS USING FINITE ELEMENT ANALYSES

Lessons Learned from Corroded Pipeline Research

The oil and gas pipeline infrastructure is aging, and a great deal of research has been conducted to understand how to evaluate the remaining strength of corroded pipes. The current technique used to assess corroded pipeline is commonly referred to as the B31G criterion (ANSI/ASME, 1986; CAN CSA, 1986). This technique, which is based on fracture mechanics considerations of a longitudinal surface crack in a pressurized cylinder, has been effective in conservatively estimating the remaining strength of corroded pipes.

Unfortunately, the B31G criterion is often conservative by a factor of two or more. Using a database of 87 experiments on corroded pipe sections (Kiefner and Vieth, 1989), Chouchaoui and Pick (1994) compared the ratio of burst pressure using B31G to the actual burst pressure from the experiments. They showed that "the mean ratio of predicted to actual burst pressure is 0.586, which on average provides a very conservative assessment. The scatter in the predictions is relatively large as ratios of actual to predicted burst pressures varied from 0.032 to 1.036 with a standard deviation of 34% of the average predicted to actual burst pressures." Because of this excess conservatism, the pipeline industry has recognized the need for an improved assessment procedure to understand the behavior of corroded pipelines.

Much of the recent research for the pipeline industry has been devoted to showing that finite element analyses can accurately predict the burst pressures of corroded pipelines. Some of the results from the pipeline research are:

- The corroded pipes tested by Kiefner and Vieth (1989) exhibited ductile failure modes, and did not fail in a brittle manner.
- Corrosion defects in pipelines are typically blunt, having a radius of the same order of magnitude as the wall thickness (Stephens and Bubenik, 1993). Corrosion defects that are blunt have a stress concentration factor that is close to one. Failure occurs at these blunt defect locations through uncontrolled plastic flow and material instability similar to necking in a tensile test specimen.
- The structural response of a corroded pipeline can be accurately predicted by finite element analyses. A detailed section of corrosion on a pipe was measured by Valenta, et al. (1996). The corrosion consisted of a very irregular shape and depth, with the length of corrosion about 629 mm long, and the depth varying between 1 and 6 mm. The deepest corrosion areas had a degraded thickness of about 50% of the original thickness. The corroded section was instrumented with strain gages, and then pressurized until it burst at 13.4 MPa. The theoretical burst pressure of a non-damaged section was about 16 MPa. Although the authors did not predict the burst pressure of the pipe using the finite element analyses, they did compare measured strains with calculated strains up to pressures of 10 MPa. This corresponded to strains in the thinned areas of about 5%, and up to these levels there was quite good agreement between the tests and analyses. The finite element mesh was fairly coarse, but was still adequate to capture the structural response of the corroded section.
- The burst pressure of a corroded pipeline can be accurately predicted by finite element analyses, using stress and/or strain based failure criteria. Chouchaoui and Pick (1992, 1993, and 1994) performed large displacement, elastic-plastic finite element analyses of single and multiple corrosion "pits", where the pits were 25 to 50 mm in diameter, and the corrosion depth is up to 80% of the wall thickness. In the finite element analyses of these pits, high stresses and strains localized in the damaged area. In these areas, the maximum engineering stress from uniaxial tensile strain-to-failure tests was a reliable failure criteria, and the calculated burst pressures were in excellent agreement with the experimental results. The authors concluded that in the corroded area under a pit, a three by three grid of second order continuum elements (twenty node bricks) was adequate to predict the failure pressures with an accuracy of 10% or better.. Similarly, an effective plastic strain criterion also gave reliable failure predictions.
- Stephens and Bubenik (1993) conclude that "corrosion defects fail after the membrane stress exceeds yielding and the defect strains sufficiently to allow uncontrolled plastic flow." When this point is reached in a static finite element analysis, the code goes unstable because the structure is no longer stable. They explain that "bending stresses have limited influence on ultimate failure." In local areas where high bending stresses are present, local plastic bending results in a redistribution of stress until the loads are carried predominantly as membrane forces.

- Popelar (1993) shows that stress in the pipe wall is almost the same for metal loss from the inside or the outside of a pipe.

Damage Mechanisms

The principal damage mechanism that is addressed in this paper is the *loss of section* phenomenon that has caused problems for the oil and gas pipeline industry. A possible failure mechanism that is not discussed in this paper is *local pitting*, or very small holes, which penetrate the wall and cause a leak. Because the amount of actual material that is corroded away is so small, there is no appreciable loss of strength in the pipe section. However, these local pits could penetrate the steel shell and cause the vessel to leak.

Types of degradation that cause the material to become brittle, such as intergranular and transgranular stress corrosion cracking and hydrogen embrittlement, do not affect the low-carbon low-strength steels used in containment structures. Radiation levels near the containment wall are low enough that radiation embrittlement does not occur. For containment structures, it is very unlikely that the small number of load cycles will be sufficient to cause fatigue failure or fatigue-related damage.

Welded Properties

When subjected to severe loads, it is common for welded structures to fail in the weld or in the heat-affected zone. However, for the low-carbon low-strength steels used in containment structures, testing has shown that failure occurs away from the weld area. Based on test data (*Structural Alloys Handbook*, 1989; Spletzer, et al., 1995), the weld is not the "weak link" in structural failure of A516 or SA212 steels. During uniaxial tension tests, these materials failed in the base metal and not in the weld zone. Of course, a significant crack in the weld zone could cause brittle fracture, just as a large crack in the containment wall could cause brittle fracture. However, corrosion damage found in actual containments has not shown this kind of degradation along weld seams.

Corroded Coupon Tests

To experimentally measure the structural degradation caused by corrosion, several 0.159 cm thick A516 Grade 70 steel plate samples were corroded and then tested to failure in uniaxial tension (Cherry, 1996). Damage to the first plate consisted of general corrosion and pitting. The average thickness decreased by about 5%, but pits and pockmarks were visibly deeper than the material surrounding them. Damage to the second plate was similar, except that the average thickness decreased by about 10%. In the third plate, the pits and pockmarks were deeper and the surface was more irregular. The average thickness of this plate decreased by about 20%.

Five standard ASTM dog-bone-shaped tensile coupons were cut from each of the corroded plates, and five coupons were taken from an uncorroded control plate. The coupons were tested to failure in uniaxial tension in accordance with ASTM Standards (1991). The uncorroded coupons reached about 24% elongation before significant necking began, and about 28% total elongation before failure occurred. Coupons that were corroded approximately 5, 10, and 20% through the thickness reached the same stress levels as the uncorroded specimens, but necking of the specimens began around 12% strain.

Up to about 12% strains, the stress versus strain curve was about the same for both the corroded and uncorroded coupons. However, plate sections that had corroded were able to carry less load because the cross-sectional area had decreased. The corroded coupons failed at about 50% of the elongation that caused failure in the uncorroded specimens. This reduction was caused by very local stress and strain concentrations around pits and on the rough uneven surfaces. In these "micro" concentration regions, the plastic strains were larger than they were in areas outside of the concentration region. Failure was initiated as a result of these very local plastic strains.

Finite Element Modeling

Several scaled model containments have been tested and analyzed over the last fifteen years (Horschel, 1992, Sammataro et al., 1992, Weatherby, 1990, Clauss, 1985). The finite element analyses of these uncorroded structures showed that failure is related to local strains, and that a critical strain failure criterion can be used to predict the onset of failure. As the material strains, voids coalesce into a flaw. At some critical strain level, the flaw reaches a critical size and a tear initiates. Most analyses that have been performed for containment structures have selected strain (or stress) based failure criteria.

Linear elastic fracture mechanics have often been used to predict critical flaw sizes in piping, in pressure vessels, and in other situations. This method has been proven and is reliable for linear elastic problems. However, it is very difficult to apply fracture mechanics methods to highly ductile materials that undergo high strains and gross plasticity. Furthermore, it is difficult to define a traditional fracture mechanics "flaw" that represents the rough uneven surface that has been observed in corroded areas of containment vessels. Finite element analyses, using strain based failure criteria, have been used successfully for uncorroded containments and for corroded pipelines. Fracture mechanics methods are difficult to apply to the corrosion damage. Therefore, a strain based failure criterion has been selected for use with finite element analyses. Sharp cracks of significant size have not been observed in corroded areas of containment structures. However, fracture mechanics methods should be considered if sharp cracks are detected during future inspections.

Strain-based failure criteria, which have been successfully used in past containment analyses, have been modified to account for corrosion effects. In a corroded region, the metal loss region is typically very irregular in both depth and extent. Therefore, the shape of the metal loss region must be idealized. In the finite element model, a corroded area is modeled using the average thickness of the degraded section. Local stress and strain concentrations that are caused by the reduced thickness and the surrounding geometry are calculated by the analysis code; the stresses and strains increase in the degraded area. The stress-strain curve used in the reduced thickness region is identical to the curve for uncorroded material, except that the critical elongation of the corroded material is decreased, as described in the following section.

In the finite element model of a large structure, it is not practical to model the uneven surface of a corroded plate because of the large number of elements that would be required. Pitted and rough surfaces consist of "micro" discontinuities that are much smaller than the elements in the mesh. These "micro" discontinuities are accounted for by applying a knockdown factor to reduce the critical elongation for material in the degraded area. However, stress concentrations

that result from geometry changes, such as a locally thinned area or a penetration, are calculated in a finite element analysis.

Failure Criteria

Failure is predicted to occur when calculated strains exceed a critical value. After a tear initiates, the failure criteria will not predict whether the tear will be unstable and propagate, resulting in catastrophic failure, or whether the crack will self-arrest and not propagate, resulting in a leak. Determining whether a crack will propagate has been tried in the past, and the results were not conclusive (Irvine and Gardner, 1983; Greimann et al., 1993). However, it appears likely that if the tear initiates in a region of high membrane strains, the tear will propagate rapidly and result in catastrophic failure. If the high strain is in a local region and the surrounding area is at a much lower strain, the failure may not be catastrophic.

Another failure criterion that is being considered, but has not been incorporated in this paper, is a displacement based criteria. Many penetrations exist in the walls of the containment structure, and excessive displacements could cause either a failure in the penetration seals, or break pipes and other items that pass through the penetrations. Many items, such as overhead cranes, are supported by the containment walls. Excessive displacement could cause these items to fall onto critical items below. However, this additional displacement failure criterion was not used in this paper.

The strain-based failure criterion that has been selected consists of applying "knockdown" factors to adjust uniaxial strain-to-failure test data. The first three factors are consistent with previous analyses (Miller, 1990, and Weatherby, 1990) and are not related to corrosion. These factors relate to the ability of the analysis model to correctly predict the structural response and to material property differences that exist in actual containments. The fourth factor has been added to account for the random effects of corrosion degradation.

Hancock et al. (1976), Mackenzie et al. (1977), Mangione (1982), and others have shown that the critical failure strain varies as the stress state changes. The first "knockdown" factor is the Hancock and Mackenzie relationship between the triaxial stress state and the failure strain. This relationship was based on test results. The second "knockdown" factor accounts for how detailed the finite element model is. For example, the element mesh size and details missing in the model affect the accuracy of the finite element prediction. This factor approaches 1.0 as the mesh size becomes small and includes all structural details. The third "knockdown" factor accounts for the fact that in an actual structure the material properties often vary from the mean by a significant margin. For example, test data from 489 specimens used in the Sequoyah containment had yield strengths that varied from the mean by $\pm 12\%$, ultimate strengths that varied from the mean by $\pm 7\%$, and elongations that varied from the mean by $\pm 22\%$. These variations are based on a normal distribution, with 90% of the test results falling within the specified deviations. The last "knockdown" factor accounts for corrosion degradation, and is based on the limited set of corroded coupon tests. The pitted and rough surface "micro" discontinuities are accounted for by this factor. When extrapolating corroded coupon test data to full scale containments, there is a considerable amount of uncertainty.

The critical effective plastic strain at which failure is predicted to occur is determined as:

$$\epsilon_{\text{failure}} = \epsilon_{\text{uniaxial}} * f_1 * f_2 * f_3 * f_4 \quad (1)$$

where

$$\begin{aligned} \epsilon_{\text{failure}} &= \text{effective plastic strain level at which failure is predicted to occur,} \\ \epsilon_{\text{uniaxial}} &= \text{elongation from uniaxial tensile strain-to-failure tests,} \\ f_1 &= \text{knockdown factor to account for multiaxial stress state,} \\ &= 1.648 * e^{-(\sigma_1 + \sigma_2 + \sigma_3) / 2 \sigma_{\text{von}}} \quad (2) \\ f_2 &= \text{knockdown factor to account for the sophistication of the analysis model,} \\ f_3 &= \text{knockdown factor to account for variable material properties,} \\ f_4 &= \text{knockdown factor to account for corrosion degradation,} \\ \sigma_{\text{von}} &= \text{von Mises effective stress,} \\ \sigma_{1,2,3} &= \text{principal stresses.} \end{aligned}$$

The f_1 , f_3 , and f_4 factors were estimated before analyses were performed. The f_2 factor was estimated after the analyses had completed. It was based on the amount of detail included in the finite element model in the critical failure region, and on analytical results such as strain gradients in the critical region. For each of the factors f_2 , f_3 , and f_4 , a best estimate has been selected, along with an upper and lower bound estimate. In the analyses in the following section, failure occurred in the cylindrical section of the containment. The f_1 factor (Table 1) is for a cylindrical section, and is not appropriate for use with other geometry shapes, such as the spherical shape of the dome. These factors (Table 1) are based on engineering judgment and the data already presented. Because the stress-strain curve is fairly flat at high plastic strain levels, a large change in strain only results in a small change in the stress. Therefore, predicted failure pressures are not very sensitive to changes in the plastic strain failure criteria, and hence to the knockdown factors.

Table 1. Knockdown Factors Used in Failure Criteria.

	Factor	Lower bound	Best estimate	Upper bound
Biaxial stress in a cylinder	f_1	0.69	0.69	0.69
Analysis sophistication	f_2	0.40	0.50	0.60
Material properties	f_3	0.78	1.00	1.22
Corrosion	f_4	0.25	0.50	0.75

Finite element analyses that were performed are described in the next section. However, two cases required analyzing a submodel in a local region where large bending strains existed. This submodel used a fine mesh with four continuum elements through the thickness. Because of the detail, the sophistication factor (f_2) was increased to 0.9 for the lower bound, best estimate, and upper bound values for those analyses.

Minimum uniaxial strain-to-failure values for elongation in a 20.3 cm (8 in.) gage length are given in the ASME code as 21% for A516 Grade 60 steel. For actual specimens, the elongation at failure is well above the 21% minimum; however, the load carrying capacity of the specimens began to decrease significantly around 21%, with total failure occurring at higher levels. Using $\epsilon_{\text{uniaxial}} = 21\%$, and the knockdown factors from Table 1, the failure strains for A516 Grade 60 steel were calculated. The lower bound, best estimate, and upper bound critical membrane strains for uncorroded material were 4.5, 7.3, and 10.6%, respectively. For material that was corroded, the lower bound, best estimate, and upper bound critical membrane strains were 1.1, 3.6, and 8.0%, respectively. The critical values allowed for bending strains were increased by 50%. In the analyses, the uncorroded failure strains were used to predict failure of the portions of the containment that were not corroded, and the corroded failure strains were used to predict failure of the corroded sections.

ANALYSES OF A "TYPICAL" PWR ICE CONDENSER CONTAINMENT

A U.S. Nuclear Regulatory Commission report (Shah et al., 1994) gives a thorough listing of nuclear power plant containment structures, typical design parameters, the materials used in the structures, environmental operating conditions, types of corrosion that can be expected for the materials involved, and the locations on the containment structures where the corrosion is likely to occur. They also provide detailed descriptions of corrosion degradation that has been observed in containments of operational nuclear power plants.

Geometry

The typical PWR Ice Condenser containment (Fig. 1) is a circular cylinder capped with a hemispherical dome, and is constructed entirely from A516 Grade 60 steel. The cylindrical section has a diameter of 35 m, and the height from its base to the top of the dome is about 53 m. The shell thickness varies from 3.49 cm at the bottom, to 1.11 cm at the top of the cylinder, and then increases back to 2.38 cm at the top of the dome. The shell is 3.81 cm thick in the vicinity of pipe penetrations, airlocks, and other openings. Welded to the exterior of the shell is a web of vertical stringers, horizontal stiffeners, and other miscellaneous structures. The vertical stringers are spaced every four degrees circumferentially, and the horizontal stiffeners are about 2.9 m apart. The shell is embedded in and anchored to a reinforced concrete basemat.

Three areas that were degraded in the finite element model were: the region around the ice basket, a ring area near the basemat, and a ring area at the upper floor level. In actual containments, the region around the ice basket has a high potential for corrosion, but the status is unknown because the area is inaccessible for inspections. During inspections, corrosion damage has been observed in PWR Ice Condenser containments near the basemat and at the upper and lower floor levels (Figs. 2 and 3).

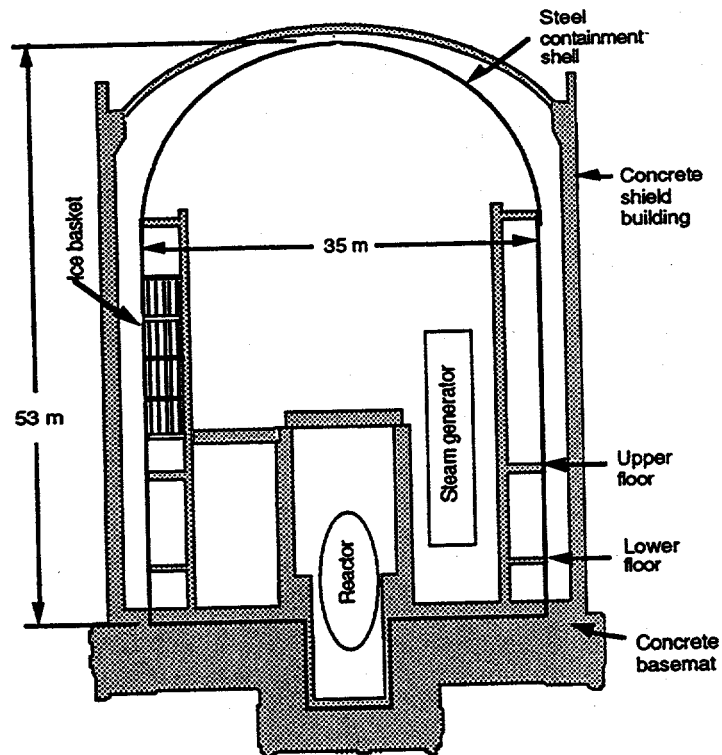


Figure 1. "Typical" PWR Ice Condenser containment.

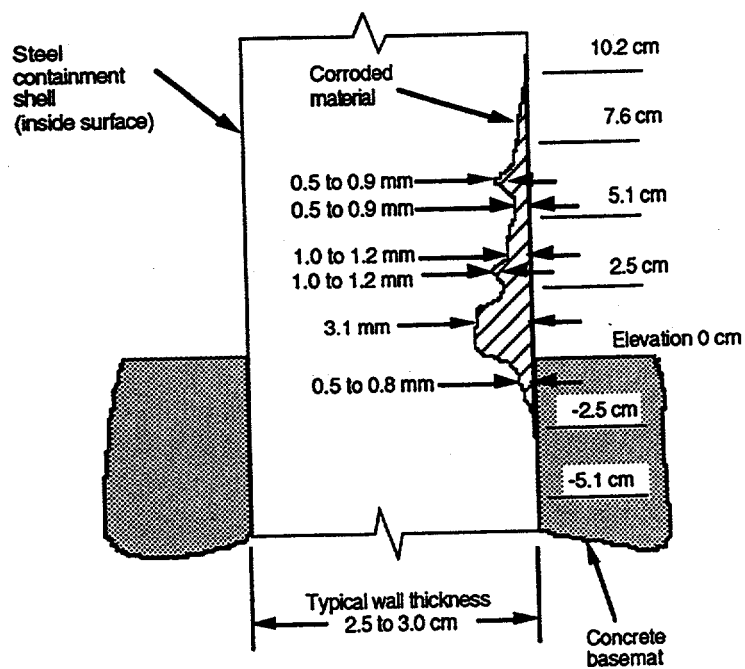


Figure 2. Observed damage to steel shell near basemat.

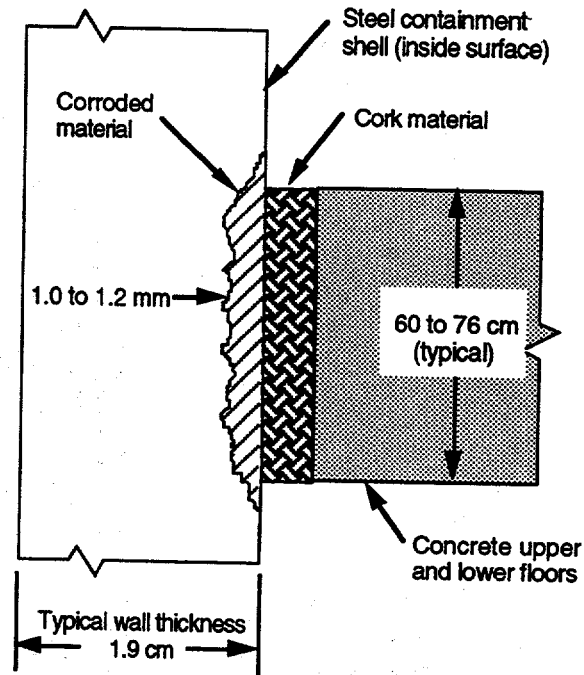


Figure 3. Observed damage near upper and lower floor.

Finite Element Model

The ABAQUS finite element model (Fig. 4) consisted of 10125 four-node quadrilateral shell elements (S4R) and 10325 nodes. It included a 53° circumferential segment of the containment, and went from the concrete basemat to the top of the dome. This segment, which had symmetry boundary conditions applied to it, was representative of a typical containment. Vertical stringers and the horizontal stiffeners were included in the model. The smallest elements had a side length of about 10 cm, while the average element had a length of about 30 cm (1° circumferentially). The concrete basemat was not modeled since it had been studied previously (Fanous et al., 1993). The penetrations were not explicitly modeled either. In previous analyses (Miller, 1990; Greimann, et al., 1984) it was found that containment failure occurred where thin wall sections met the thicker plate sections that the penetrations passed through (Clauss, 1985; Greimann et al., 1987). In these studies, plastic yielding occurred in the thinner plate sections, and not in the thickened plates. Bellows were addressed under a previous program (Lambert, et al., 1995).

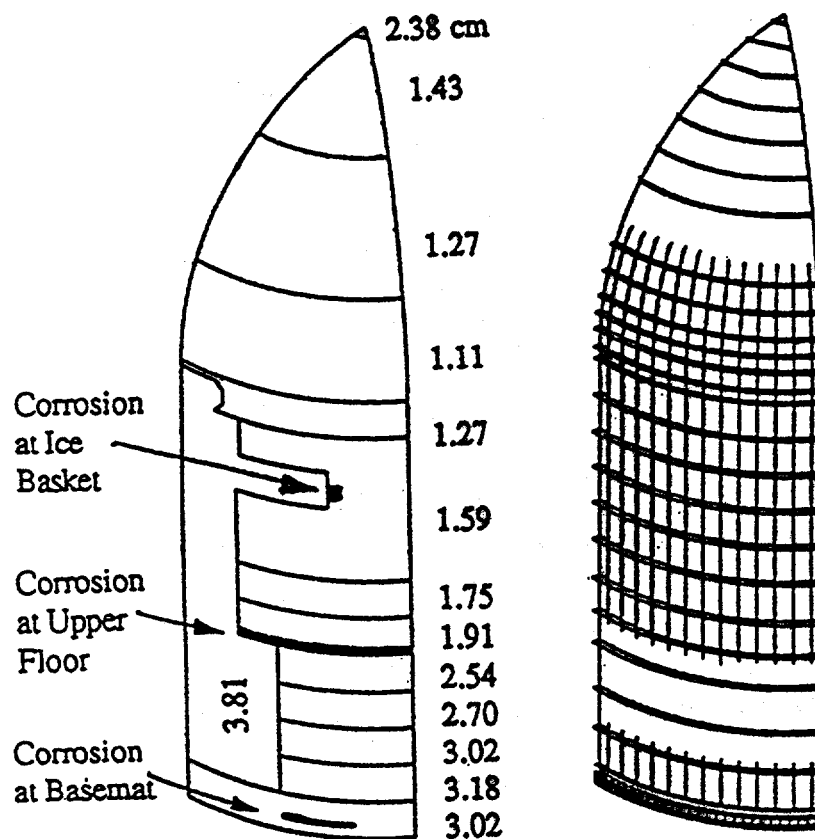


Figure 4. Finite element model shell thicknesses and stringer and stiffener locations.

The containment was modeled to determine failure level and location under several different degraded conditions. Three areas on the containment surface that were degraded (Fig. 4) correspond to the damaged or susceptible areas identified at nuclear power plants. In these areas corrosion was modeled by thinning shell elements and reducing the critical plastic failure strains. The depth of the corrosion was selected to ensure that failure occurred in the degraded area. Greater than 50% through the thickness damage was required at two locations to cause a failure in the degraded area. The containment was analyzed in seven different configurations, with:

- No corrosion present.
- Corrosion near the top of the ice basket, with a 10% through the thickness corroded area of 1.09 m high by 0.91 m circumferentially. In operational containments, this area is susceptible to corrosion, but is inaccessible and does not get inspected. Analyses show this is the area of highest strains on an uncorroded containment, and the expected failure location.
- Corrosion near the top of the ice basket, as described above, except the damage is 25% through the thickness.

- Corrosion in the steel shell at the upper floor level, with a 50% through the thickness corroded area of 0.81 m high by 11.94 m around the circumference. Corrosion has been found in this location during inspections at the Catawba and McGuire plants.
- Corrosion at the upper floor level, as described above, except the damage is 65% through the thickness.
- Corrosion in the steel shell near the concrete basemat, with a 50% through the thickness corroded area of 0.102 m high by 3.99 m around the circumference. Corrosion has been observed here during inspections at the Catawba and McGuire plants.
- Corrosion in the concrete basemat region, as described above, except the damage is 65% through the thickness.

Material Properties

Previous finite element analyses have been performed for the Sequoyah containment building (Miller, 1990). To support that and similar efforts, work-hardening data was obtained by testing A516 Grade 70 steel at 22, 93, 149, and 204°C (Fatigue Technology Inc., 1988). A516 Grade 60 temperature-dependent material properties were estimated (Fig. 5) by adjusting for the difference in yield strength between Grade 70 and Grade 60 steel. The coefficient of thermal expansion of the steel was determined to be $11.3 \times 10^{-6}/^{\circ}\text{C}$ over the range from 22 to 204°C.

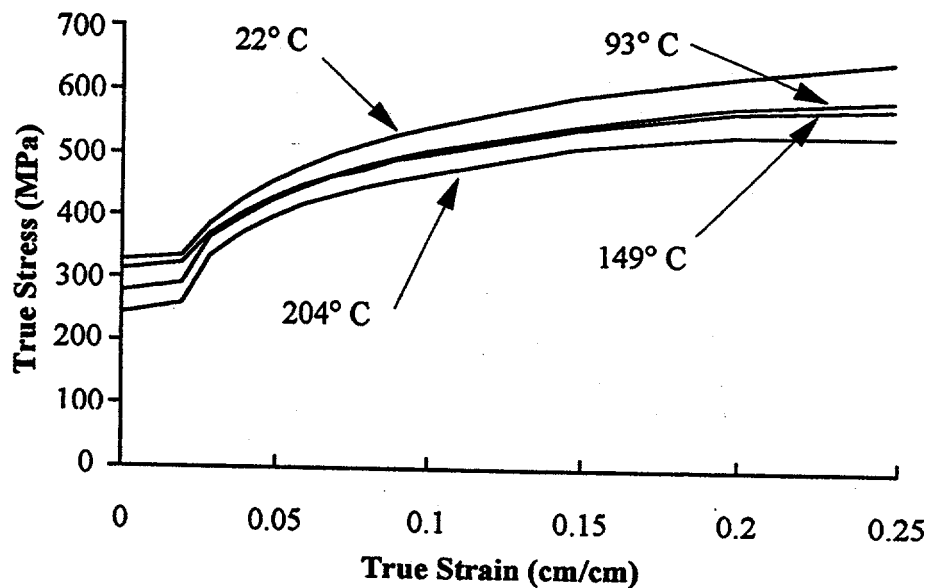


Figure 5. A516 Grade 60 temperature dependence.

Loads

Each model was loaded with quasi-static internal pressure that increased monotonically. During many postulated accidents, the pressure is caused by water turning into steam. Therefore, a thermal load was simultaneously applied to all steel parts above the concrete basemat; the temperature of the containment shell followed a saturated steam pressure vs. temperature

relationship (Fig. 6). An initial stress-free state was assumed to exist at 22°C. As the pressure was increased, the temperature at every node in the model that was above the concrete basemat was increased to correspond to the saturated steam relationship.

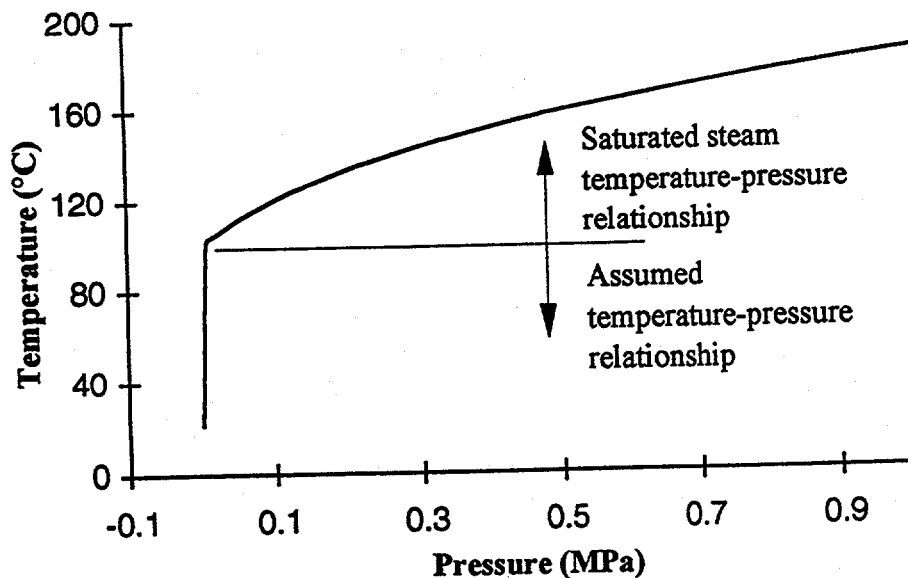


Figure 6. Saturated steam temperature-pressure relationship.

Analysis Results

Analyses were performed using the ABAQUS finite element code. Elements in the model did not automatically fail when they reached the critical strain values. Instead, failure predictions were assessed during post processing. Failure was predicted when any element in the model reached the critical strain level. For each analysis, failure predictions consisted of a "lower bound", "best estimate", and "upper bound" failure pressure.

The effective plastic strains for the uncorroded containment (Figs. 7 and 8) showed a few areas that experienced large plastic membrane strains. For the uncorroded containment, failure would be expected to occur at the high strain location around the ice basket. At this location a 3.81 cm thick plate protruded into a 1.59 cm thick area and caused large membrane strains in the thinner plate. A few other locations on the model with a thick section protruding into a thinner area also showed a region of large membrane strains in the thinner plate near the discontinuity. These strains were considerably larger than free field strains in the thin plate. However, many areas where thick plates transitioned to thin plates did not experience larger-than-free-field strains in the region near the weld joint. From Fig. 7 it can be seen that large plastic strains were only reached in a few regions, and that the global, free-field strains were considerably lower.

The analyses that included "corrosion" had higher strains in the locally thinned areas than corresponding strains in the undegraded case. Although the strains were higher in the locally thinned areas, the global responses were very similar to the response of a containment with no corrosion damage.

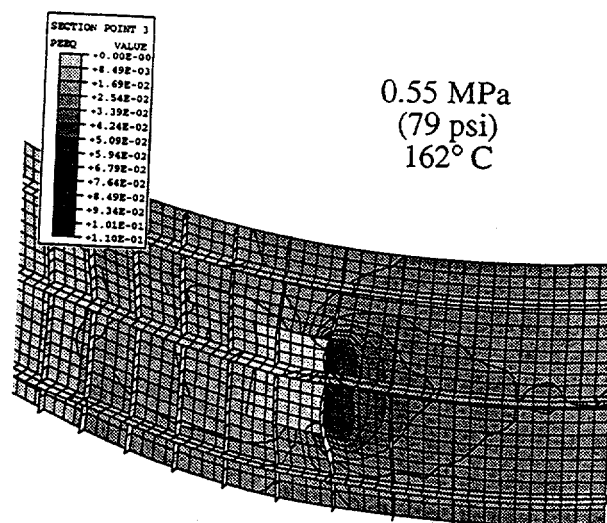
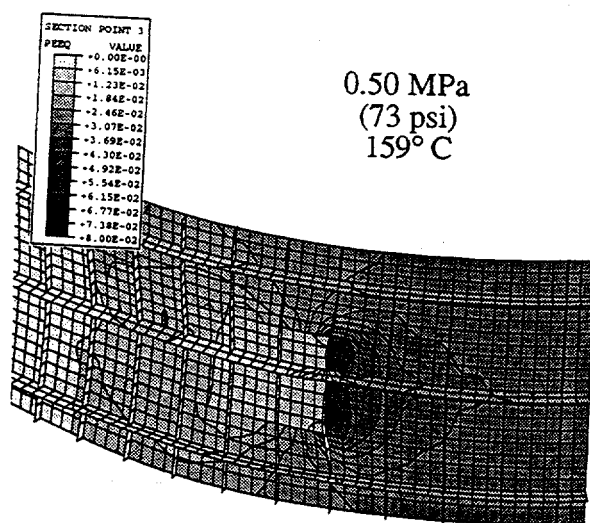
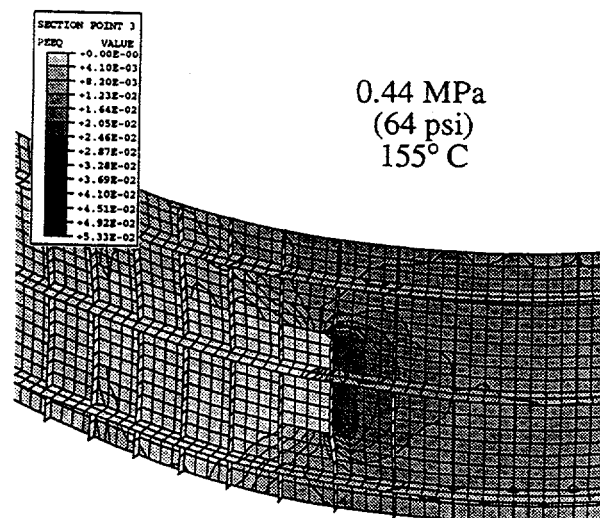
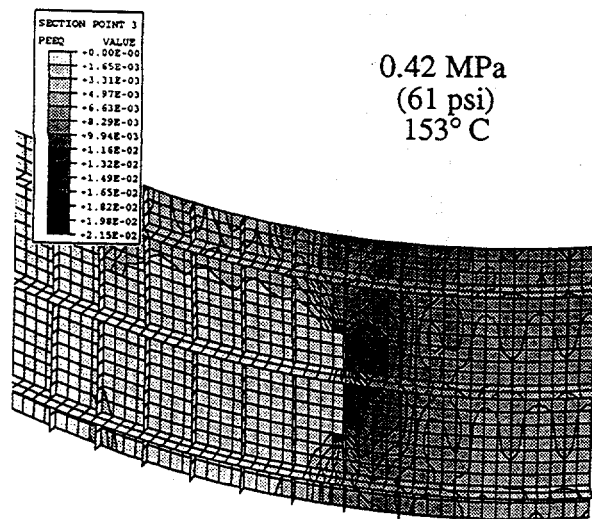
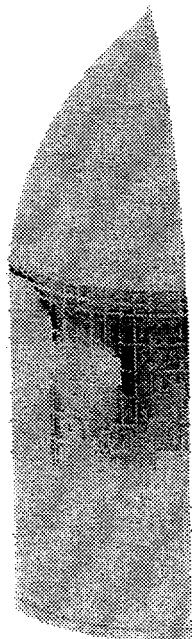
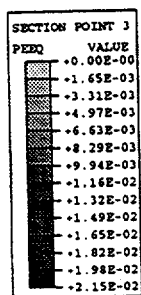
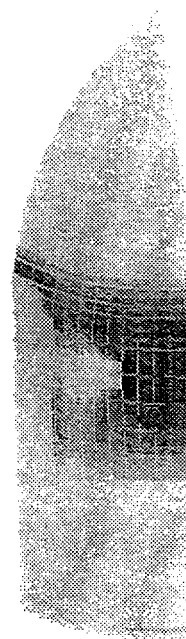
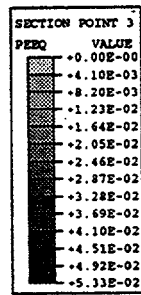


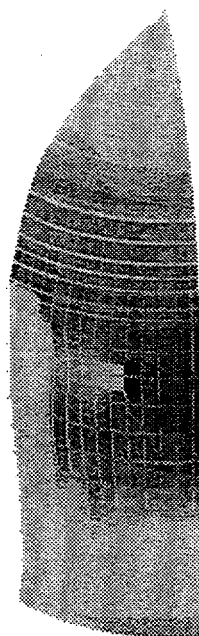
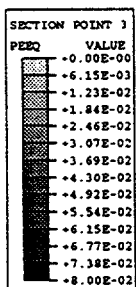
Figure 7. Plastic membrane strain (uncorroded containment) at highest strain region by ice basket.



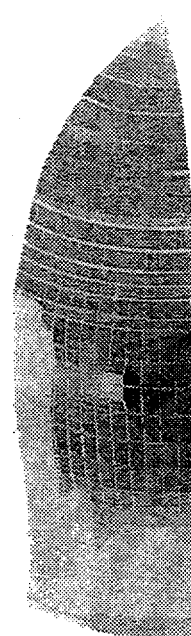
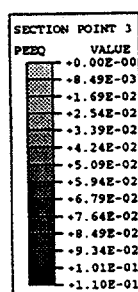
0.42 MPa
(61 psi)
153° C



0.44 MPa
(64 psi)
155° C



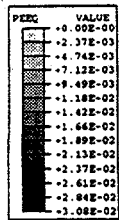
0.50 MPa
(73 psi)
159° C



0.55 MPa
(79 psi)
162° C

Figure 8. Plastic membrane strain (uncorrod containment) with displacements magnified by a factor of 5.

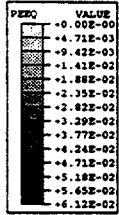
Because of significant bending near the basemat, a finely meshed axisymmetric submodel of the area was analyzed. Since the finite element mesh was much finer, the "sophistication" knockdown factor (f_2) was increased to 0.9 for the lower bound, best estimate, and upper bound cases. This resulted in critical values for bending strains in the submodel of 3.8, 9.8, and 18% for the lower, mid, and upper bound criteria. Fig. 9 shows the strains that were calculated by the submodel for the case of 50% corrosion. As can be seen, the bending strains were significantly higher than the membrane strains.



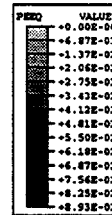
0.22 MPa (32 psi) 137 C



0.33 MPa (48 psi) 147 C



0.46 MPa (67 psi) 156 C



0.55 MPa (79 psi) 162 C

Figure 9. Plastic strain (bending and membrane) with the shell near the basemat corroded 50%

Predicted failure results (Table 2) show that corrosion in the area of highest strain will significantly reduce the pressure capacity of the vessel, while considerable corrosion in other areas can be tolerated without reducing the pressure capacity. The design pressure for the containment modeled was 0.074 MPa (10.8 psi).

The structural response of the first five cases (Table 2) was primarily membrane. Membrane forces in the cylindrical portion of the structure were twice as large in the circumferential direction as they were in the vertical direction. This is a known behavior of cylindrical sections. In the first three cases, internal pressure caused the structure to radially expand until failure occurred in the circumferential direction. Even while the structure was plastically flowing in the hoop direction, there was minimal plastic growth in the vertical direction. For these three cases, failure was predicted to occur near the top of the ice basket as a result of large hoop strains. In cases 2 and 3, the corrosion was located in the area of highest plastic strain. As expected, even small amounts of corrosion in high strain regions caused failure to occur at lower pressures. In cases 4 and 5, the thinned section was only 0.81 m high, but extended 11.9 m in the circumferential direction. The thicker sections above and below this degraded area were stiff enough that they did not undergo large hoop strains, and this support prevented the thinned section from experiencing large plastic hoop strains. It was not until the thickness of the corroded section was decreased by a factor of two that the vertical component of stress became large enough to cause large plastic membrane strains in the vertical direction, and failure was predicted at this location. For cases 6 and 7, the steel shell was relatively thick near the basemat and was embedded in the concrete floor. Circumferential straining in the thinned

Table 2. Predicted Failure Pressures

Case No.	Description	Failure Pressure		
		Lower bound	Best estimate	Upper bound
1	no corrosion	0.43 MPa (63 psi)	0.47 MPa (68 psi)	0.54 MPa (78 psi)
2	10% corrosion at ice basket	0.40 (58)	0.43 (63)	0.48 (69)
3	25% corrosion at ice basket	0.39 (56)	0.43 (62)	0.45 (65)
4	50% corrosion at upper floor	0.43 (62)	0.47 (68)	*
5	65% corrosion at upper floor	0.35 (51)	0.44 (64)	0.47 (68)
6	50% corrosion at basemat	0.30 (43)	*	*
7	65% corrosion at basemat	0.28 (41)	0.47 (68)	*

* Failure is not predicted to occur at the corroded location.

section at the basemat was limited because of support from the basemat and the thick plate above the reduced area. It was not until the thickness of the corroded section was decreased by a factor of two that the vertical component of stress became large enough to cause large plastic strains in the vertical direction. In this case, however, the peak strains were a result of membrane and bending in the vertical direction.

CONCLUSIONS

The corrosion damage in existing containments has often been in a horizontal plane, such as along the upper and lower floors and at the basemat level (Figs. 2 and 3). For a cylindrical structure, a narrow band of thinned material that extends around the circumference can often be tolerated because the surrounding structure is often stiff enough to prevent the degraded area from large plastic hoop strains. However, if a narrow band of corrosion occurred in the vertical direction on a cylindrical containment, the reduction in capacity would be more severe. This is because internal pressure causes a larger membrane force in the hoop direction than in the vertical direction. In the corrosion that was modeled around the ice basket, failure occurred through large plastic strains that grew circumferentially. The geometry of the structure caused large membrane hoop strains in the thinned area, and the structure expanded in the radial direction. Therefore, any amount of corrosion near the ice basket high strain region degraded the load-carrying capacity.

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James Costello and Wallace Norris, from the U.S. Nuclear Regulatory Commission, have been instrumental in developing this program.

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This three-volume report contains papers presented at the Twenty-Fourth Water Reactor Safety Information Meeting held at the Bethesda Marriott Hotel, Bethesda, Maryland, October 21-23, 1996. 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 papers presented by researchers from Finland, France, Japan, Norway, Russia and the United Kingdom. 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.

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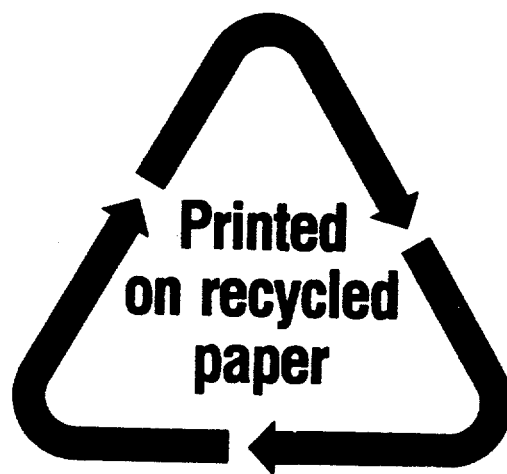
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