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UNITED STATES OF AMERICA
NUCLEAR REGULATORY COMMISSION

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SECRETARY
OF SERVICE

BEFORE THE ATOMIC SAFETY AND LICENSING BOARD

In the Matter of

ARMED FORCES RADIOBIOLOGY
RESEARCH INSTITUTE

(TRIGA-Type Research Reactor)

Docket No. 50-170

(Renewal of Facility
License No. R-84)

LICENSEE'S MOTION FOR PARTIAL SUMMARY DISPOSITION

The Licensee submits this motion pursuant to 10 C.F.R. 2.749 and urges the Atomic Safety and Licensing Board to determine that, at least as to some of the issues being litigated in this proceeding, there is no longer any genuine dispute as to the material facts involved and that as a consequence there can be no genuine issue remaining to be heard.

As is indicated in the Board's Memorandum and Order of January 28, 1983, there are ten general areas in dispute. Those ten general areas consist in some instances of several factual issues each of which involve several more subissues. This motion, except as will be indicated shortly, discusses those issues in the same order (and with the same titles) as they appear in the Appendix to the Board's January 28, 1983, Order. One exception to this procedure is the need to depart (very slightly) from the titling approach since some of the subparts of the ten general areas of dispute require treatment at some length. A second exception

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to this approach relates to Contention 3, Emergency Plan. The Board has previously indicated that motions concerning the Emergency Plan would not be entertained prior to publication by the NRC Staff of its evaluation of the Emergency Plan. Hence, consideration of issues associated with the Emergency Plan must be deferred for the present.

Each of the contentions discussed will be addressed in summary fashion in the body of the motion. That is, the issue(s) raised by the contentions will be restated so as to focus the Board's attention on the precise areas of disagreement. This will be followed by a synopsis of the facts pertinent to the issue and Licensee's analysis of their significance. In addition, as to each conveniently segregable set of issues identified in the body of the brief, there is appended to the brief a fuller discussion (to the extent necessary) of the logic associated with the Licensee's analysis of the significance of the issue.¹ Stated another way, the body of the motion presents an executive summary of those matters treated at length in the attachments.

CONTENTION 1 - ACCIDENTS I

This contention deals with the "Fuel element clad failure accident DBA." Intervenor contends that Licensee's analysis of this Design Basis Accident is erroneous because Licensee assumes that such clad failure would occur at

^{1/} Attachment 1 contains information concerning the professional credentials of Licensee's principal Affiant.

a peak fuel element temperature of less than 100°C. Intervenor contends that such clad failure would be more likely to occur at fuel temperatures greater than 400°C and thus result in a greater gap activity and fission product release than the HSR (or more correctly) Safety Analysis Report (SAR)² postulates.

As the issue is now framed, there can really be no genuine dispute about what peak fuel element temperature was used in the preparation of the Safety Analysis Report (SAR). This temperature, while not identified in the SAR in degrees, can readily be derived from the percentage of gap activity. This derivation is articulated in detail in Attachment 2. The SAR (at page 6-12) clearly reflects that a fractional release of 0.1% was used. This gap activity is associated with a fuel temperature greater than or approximately equal to 600°C. Since Intervenor's concern is with respect to clad failures at fuel temperatures in excess of 400°C and with realistic gap activities and releases for a fuel element clad failure accident DBA, all of which the Licensee has appropriately considered, this contention must be summarily dismissed.

CONTENTION 2 - ACCIDENTS II(1) -

Fuel Element Storage Rack Failure

This contention deals with a single fuel element storage rack failure assumed to be fully-loaded with 12

^{2/} The language of the contentions refers to the HSR (Hazard Summary Report) which was filed with the license application. This document was all that was available at the time the contentions were drafted. Since that time, the Safety Analysis Report has been prepared and filed. The SAR replaces in terms of significance, at least, the HSR. References in the motion to the SAR are thus understood to be synonymous with the less comprehensive and older HSR mentioned in the contentions. CNRS has indicated that this point of clarification is correct. See Dr. Stillman's December 18, 1982, Deposition at pp. 17 line 16 through 18 line 1.

stainless-steel clad TRIGA fuel elements. Intervenor contends that the SAR does not provide reasonable assurance that such an accident cannot occur because: 1) no criticality calculations for a twelve element configuration are provided and, 2) no statement of experience is cited to indicate/support the Licensee's claim that it takes approximately 67 stainless-steel clad TRIGA fuel elements to achieve criticality. In addition, the Intervenor contends that a storage rack failure accident is of a different kind and a greater severity than accidents treated in the SAR and should be categorized as a DBA.

While there may have been some doubt at the time that this contention was drafted concerning whether or not twelve elements in a worst case neutronic configuration could achieve criticality and while the Intervenor may not have been aware at that time of the calculations which demonstrate the number of elements of the type used at AFRI needed to achieve criticality, the record is now complete. The unrebutted evidence of record (as a result of Licensee's answers to Intervenor's interrogatories) demonstrates that a fully-loaded storage rack (containing 12 fuel elements) cannot under any circumstances achieve criticality. Moreover, the record now contains ample data demonstrating that approximately 69 stainless-steel clad TRIGA fuel elements are necessary to achieve criticality. The data in question is summarized in Attachment 3.

In light of the facts now of record, it is obvious that it is incredible that an unrestrained critical or supercritical configuration of stainless-steel clad TRIGA elements in the reactor pool could be achieved as a result of storage rack(s) failure. What's more, even if criticality were somehow achieved, it would be at the bottom of the reactor pool. There would thus be no deleterious consequences for the reactor staff or the public.

Licensee submits that Intervenor's contention expresses specific concerns that have been adequately addressed by the Licensee. Specifically, the calculations, analysis, and supporting documentation, the absence of which cites as a basis for its contention, have been presented and do, in fact, provide adequate assurances. Further, Intervenor's claim that such incidents represent accidents of a different kind and greater severity than those treated in the Licensee's Safety Analysis Report (SAR) is totally without support in fact. Therefore, based on the undisputed evidence of record, this contention must be summarily dismissed.

CONTENTION 2 - ACCIDENTS II(2) -

Failure of an Experiment

This contention deals with an experiment failure concurrent with a malfunction of confinement safeguards. Intervenor contends that such an accident could occur with releases potentially in excess of regulatory limits and submits that such accidents are of a different kind and greater severity than those treated in the AFRRI SAR

and, therefore should be more properly designated as DBAs.

There never was any significant factual dispute regarding this contention. In preparing this contention the Intervenor obviously consulted NRC Docket 50-170 and recorded what it believed to be the essential facts surrounding past occurrences at AFRRI. Each of the eleven inconsequential malfunctions occurred. Not one of them, however, is as significant as the Intervenor would have the Board believe. The critical point that must be understood in connection with this contention is that the Intervenor has failed to make the connection between occurrences such as these and releases to the environment. The hurdle which the Intervenor has failed to pass is one of causation as is amply addressed in Attachment 4 (which discusses each of the malfunctions cited by the Intervenor).

The Licensee has illustrated the extreme unlikelihood, and often inappropriateness, of the Intervenor's cited malfunctions with respect to their relationship or importance to confinement isolation, source term generation, and environmental release. Licensee submits that malfunctions and failures can occur but that they are extremely unlikely. If one considers multiple, independent, and concurrent events--which are, even by the Intervenor's own admission, necessary for environmental releases to actually occur, they become incredible. Licensee further submits that

its own history of safe, reliable operation over the last 21 years is evidence more convincing than anyone could ever present or postulate--particularly in comparison with a new licensing action for a similar facility. Moreover, Licensee has demonstrated its in-place system of reviewing, approving, and limiting experiments and irradiations of materials with a view towards limiting the consequences of a release, should one occur. And finally, Licensee has addressed a worst-case experiment failure with an assumed total release to the unrestricted environment from the standpoint of assessing worst-case consequences; and these consequences are insignificant by themselves and certainly miniscule with respect to other accidents that are also treated in the AFRRI SAR.

In short, Licensee submits that it has adequately addressed experiment failures with an assumed total release to the unrestricted environment. Further, Licensee submits that Intervenor's claim that such accidents are of a different kind and greater severity than those accidents treated in the AFRRI SAR is totally without support in fact. Moreover, Licensee has demonstrated working (and proven) systems or mechanisms to: 1) adequately identify malfunctions and failures in a timely fashion; 2) adequately provide backup systems to protect single malfunctions/failures from having an impact; 3) limit the probability of single

malfunctions/failures to a reasonable level; 4) make multiple concurrent malfunctions/failures extremely unlikely; and 5) ensure adequate review and limitation of materials to be irradiated, illustrating that the consequences of a release, should one occur, have been adequately evaluated and have minimal impact on the unrestricted environment and the general public. Therefore, based on the undisputed evidence of record, this contention must be dismissed.

CONTENTION 2 - ACCIDENTS II(3) -

Negative Temperature Coefficient

The Intervenor contends that Licensee has failed to demonstrate that the TRIGA reactor's negative temperature coefficient of reactivity remains negative when hydrogen is presumed lost from damaged TRIGA fuel elements. This must be so, since the Intervenor claims that Licensee has failed to demonstrate that the negative temperature coefficient will automatically shut down the reactor; the only way in which this could occur is if the temperature coefficient of reactivity somehow becomes zero or positive with a presumed loss of hydrogen from damaged TRIGA fuel elements.

The Intervenor has expanded (somewhat) upon its theories in response to interrogatories and in answers to questions posed during Dr. Stillman's deposition. As is evident from the Affidavit of Joseph A. Sholtis included in Attachment 5, the evidentiary basis for this contention is only partly present. That is, there are a total of three independent

scientific contributors (each of which is negative) associated with the TRIGA reactor's inherent negative temperature coefficient of reactivity which acts as an effective reactor safeguard. Dr. Stillman has addressed only one of those three contributors. While the Licensee cannot fully agree that Dr. Stillman's analysis is correct as to the one contributor in this area that he attempts to answer (since his postulated loss of hydrogen introduces negative reactivity), we will assume for purposes of discussion that he is. Given that assumption, there is no real disagreement concerning the scientific facts involved. That is, the Licensee has previously provided (in response to CNRS's Interrogatory 6) evidence which addresses all three major scientific contributors. This prior submission coupled with the affidavit in Attachment 5 provides ample basis upon which to permit the Board to decide.

In summary, Licensee has demonstrated that the TRIGA reactor's temperature coefficient of reactivity will always be negative and inherent, regardless of whether the TRIGA fuel is damaged and hydrogen is presumed lost or not. Licensee has also illustrated the extreme unlikelihood and difficulty in removing hydrogen from TRIGA fuel which, even if it were presumed to occur, would still not force the TRIGA reactor's overall temperature coefficient of reactivity to a zero or positive value. Licensee has also demonstrated that each TRIGA fuel element in the core, which is presumed damaged with an associated loss

of hydrogen, will contribute less and less (with increasing hydrogen loss) to the core's neutron population, power level, and fission density. Thus, each will have a suppressed neutron population, power level, fission density, and fuel temperature (in comparison with the other undamaged TRIGA elements in core), which will be suppressed more and more with increasing hydrogen loss. Thus, the consequences of accidents stemming from damaged TRIGA fuel where hydrogen is presumed lost simply cannot become more severe since conditions are not aggravated but actually reduced in these damaged elements. Finally, failed TRIGA fuel accidents are not of a different kind than those accidents that are treated in the AFRRI SAR, since clad failures are explicitly treated in the AFRRI SAR. Therefore, Licensee submits that Intervenor's contention is totally without support and must be dismissed.

CONTENTION 2 - ACCIDENTS II(4) -

Multiple Cladding Failure Accidents

This contention deals with multiple fuel element clad failures occurring concurrently in time. Intervenor contends that concurrent, multiple clad failures have not been considered in the SAR, and further contends that such an accident could result from cladding material defects, an uncontrolled power excursion, a LOCA, sabotage, aircraft collision, or a natural "Act-of-God" accident. The Intervenor also contends that a concurrent multiple clad failure accident is of a different kind and greater severity than

those accidents that are treated in the AFRPI SAR and should be more properly designated as a DBA. In essence, therefore, the Intervenor contends that concurrent multiple clad failures due to their postulated causal mechanisms are in fact credible.

This entire contention constitutes an attack on Licensee's SAR and NRC's judgment as expressed in its Safety Evaluation Report (SER) without basis in fact. This contention is built entirely upon conjecture for which supporting evidence in fact has never been provided. The Intervenor claims that concurrent multiple clad failure events can result, for example, from clad defects, yet the Intervenor has characterized such an occurrence as "very unlikely."

(See page 108 lines 19 through 25, inclusive of the transcript of Dr. Stillman's deposition in New York on 18 Dec 82.)

Moreover, when the Intervenor was asked by the Licensee in its first-round interrogatory #24b, "Have there been any prior multiple fuel element cladding failure accidents in any TRIGA reactor?", regardless of the cause, the Intervenor stated, "Not that we know of." The fact of the matter is that there has never been a concurrent multiple clad failure accident in the entire history of TRIGA reactors from any cause. Moreover, concurrent multiple clad failure accidents are not viewed (by Licensee or the NRC) as being credible events in a TRIGA reactor and, therefore, designation of them as DBAs would be absurd.

In short, the evidence is not, to the extent that it is, really in dispute. The Licensee recognizes the remote theoretical possibility that multiple cladding failures can occur (see Attachment 6). The Intervenor suggests but never demonstrates that a number of mechanisms can in fact produce multiple cladding failures. What is missing is some sort of credible causal connection between a postulated initiator and multiple cladding failures. The Licensee submits that this causal connection is critical to this contention and in its absence the contention must be dismissed.

CONTENTION 4 - ROUTINE EMISSIONS I

This contention attempts to show that radioactive materials produced from TRIGA operations are released to the environment in violation of federal (10 C.F.R. 20) guidelines. Several examples purport to show that these guidelines are exceeded because either the equipment, methods, or reporting systems are not adequate to detect violations or that limits are, in spite of procedures used, exceeded.

The Intervenor alleges that AFRRRI has "not demonstrated that airborne and waterborne radioactive emissions from routine operations and disposal of solid wastes will be maintained within the limits of 10 C.F.R. 20." In support of this claim the Intervenor states that environmental monitoring is inadequate to determine radiation doses

to the public due to inhalation or ingestion because (a) film dosimetry detects only external gamma radiation, (b) the particulate radioactivity monitor for airborne effluents (a pancake - probe GM counter) is not isokinetic and therefore cannot be used for meaningful evaluations, (c) a beta self absorption factor was omitted from calculations or environmental analysis and (d) a model used to derive its dose assessments to the environment is not realistic. These statements, however, fail to show inadequate environmental monitoring. The succeeding paragraphs will delineate the errors associated with each of these allegations.

AFRRI does not use film dosimetry for environmental monitoring. The Intervenor's answer to Licensee's interrogatory number 27a.1 states: "The use of film to detect external gamma radiation is a technique that is much inferior to the use of thermoluminescent dosimeters, whose sensitivity is much greater and far more reliable." AFRRI has used thermoluminescent dosimeters for years and is in fact pleased that Intervenor's expert witness, Dr. Ernest Sternglass, agrees that the system in use is a more sensitive, reliable system.

The air particulate monitoring system is isokinetic. The Licensee's answer to Intervenor's Interrogatory 28 shows that the particulate monitoring system is in fact

isokinetic and is therefore reliable for particulate sampling; it is used even though it is not required by the license. Indeed, use of an isokinetic particulate air sampling system provides for "meaningful evaluation" of air being exhausted from the reactor facility and generates the "better data" which the Intervenor says should be available. AFRRI does in fact continuously sample air downstream from the high efficiency particulate air filters through an isokinetic sampling system and in several years of sampling has not recorded a single instance of escaped particulate isotopes exceeding 10 C.F.R. 20 limits. This sampling history includes, of course, "the small amounts of particulate material mixed with the larger amounts of Argon" that the Intervenor feels is of concern.

The Intervenor cites an NRC-cited calculational omission concerning a beta self-absorption factor. In addition, the Intervenor suggests that its lack of information on how quarterly environmental samples of water, soil, and vegetation are prepared and analyzed proves AFRRI's environmental monitoring is inadequate. The NRC did discover that measurements were made without the use of a beta self-absorption correction factor. However, even with the correction factor applied, all releases were well below all regulatory requirements. There was at no time a significant possibility of exceeding regulatory limits since the standard procedure requires specific radionuclide

analysis if gross concentrations exceed one tenth of the regulatory limits for unspecified nuclides. As far as environmental sampling methods are concerned, contrary to the implications in the stated contention, no items of non-compliance were found by the NRC during inspections regarding the methods for preparing and analyzing samples or the instrumentation used for these purposes.

The concentric cylinder set model, despite the statement made in this contention by the Intervenor, is not used to "derive its dose assessments to the environment and from which it concludes its effluents are within regulatory limits." In fact, the entire statement is incorrect. The concentric cylinder set model only supplements environmental TLD's--it is not itself used to determine compliance with any regulatory limits. In addition, no responsible organization including the NRC, has found the model to be unrealistic and the Intervenor has yet to describe what it finds to be unrealistic about the model. (See NUREG-0851, "Nomograms for Evaluation of Doses from Finite Noble Gas Clouds," W. J. Pasciak, USNRC, January, 1983, pp. 227-240.)

This contention also expresses great concern about a small water escape line from the original AFRRRI stack. The line, which contained a water trap, allowed rain water entering the old stack to drain away. The line led from

the base of the stack access, across an equipment area and through a wall to the outside. When a new, larger stack was installed, the old stack was rerouted so that rain no longer entered this section of the stack. The line, which then became unnecessary, was removed and the exit point from the stack base was capped. Incidentally, an analysis (conducted upon discovery of this "problem") of the air flow in the stack and at the location of the exit (more than 3 feet below the normal air flow path) shows that it is not "highly probable" that releases in excess of 10 C.F.R. 20 Appendix B occurred. In fact, just the opposite is true. It is highly improbable that any releases occurred much less any that would have exceeded 10 C.F.R. 20.

In further support of its allegation that the Licensee's environmental protections are inadequate, the Intervenor states that airborne release reports for 1962, 1963, and 1964 show that releases from the AFRRI stack exceeded the MPC concentrations for unrestricted areas. A summary report prepared from available data in 1972 failed to show any releases that could violate NRC restrictions. (This documented evidence has been reviewed during many subsequent NRC inspections and would surely have resulted in a Notice of Violation from the NRC had such a violation actually occurred.) How a letter sent (6 Oct 1961) 8 months before the AFRRI reactor first went critical can show evidence of isotopic release to the environment greater

than that allowed by federal guidelines is certainly curious. The fact that the Intervenor continues to express concern with twenty year old data suggests that AFRRRI is succeeding in maintaining its environmental releases at commendable, not reprehensible, levels. Even in the "worst" of times (1963 and 1964), the evidence of record demonstrates compliance with 10 C.F.R. Part 20 limits (not noncompliance as suggested by the Intervenor in its statement that whole body doses in unrestricted areas exceeded 0.5 rem).

In summary, the evidence before the Board clearly demonstrates that the Licensee's environmental monitoring methods, equipment, and (most importantly) results are fully in accord with the regulatory requirements of the Commission. Hence, the Licensee submits that this contention must be dismissed.

CONTENTION 5 - NEPA I

CONTENTION 6 - NEPA II

These two contentions are directed principally to the attention of the NRC Staff. Both contentions allege defective compliance by the Staff with the provisions of the National Environmental Policy Act (NEPA) found at 42 U.S.C. 4321 et seq. The statute requires an evaluation of some sort in connection with "... major Federal actions significantly affecting the quality of the human environment ..." (42 U.S.C. 4332(2)(c)). The NRC has, by regulation (10 C.F.R. Part 51), established a scheme by which it decides what level of evaluation is required in a particular

licensing proceeding. The NRC Staff accomplished (in January of 1982) an "environmental impact appraisal" (EIA) in which it documented its basis for a "negative declaration." That is, based on the EIA, the Staff determined that an "environmental impact statement" (EIS), which is a detailed statement prepared by the NRC to comply with the provision of NEPA cited above, is not required prior to renewal of this license. In making this determination, the NRC Staff had before it, among other things, the "environmental report" submitted by the Licensee as part of its application for license renewal as well as a twenty year historical record on this particular facility.

The Staff then properly applied 10 C.F.R. 51.5 to the relevant environmental facts. That section identifies eleven instances in which an EIS is required. All of the specifically identified instances are clearly of far greater significance than the renewal of a license for a comparatively small research reactor. The twelfth instance is a "catch-all" instance in which actions not specifically identified may be subjected to the detailed analysis of an EIS. The Staff evidently determined that this action was not of such a magnitude to require an EIS.

The Intervenor asserts that, based on an inadequately prepared EIA, the Staff erroneously concluded that an EIS was not required. It should be recognized that these contentions were both prepared well before the Staff had prepared and published its EIA. The Licensee submits

that, with the publication of the EIA, the NRC Staff has properly discharged its duties under NEPA. Hence, Contention 6 (NEPA II) should be dismissed. Contention 7 (NEPA I) should likewise be dismissed since it relies for its analysis of the requirement for an EIS (presumably under 10 C.F.R. 51.5(a)(12)) on the totality of the Intervenor's other contentions, which as is evident from this motion, should also be dismissed.

CONTENTION 7 - SECURITY

This contention suggests that, for two categories of reasons, physical security at AFRI is so inadequate that the reactor license should not be renewed. First, the Intervenor asserts that the Physical Security Plan is inadequate.³ Second, the Intervenor asserts (citing examples) that the Licensee's "history of security violations" demonstrates that the "controlled access areas" are ineffectively protected.

As the Board noted in its Order of August 31, 1981, at page 13, the security issues were to be restricted to the building at AFRI (Building #42) in which the reactor

^{3/} This portion of the contention may well have been dropped by the Intervenor. The Physical Security Plan prepared by the Licensee and subsequently approved by the NRC is protected, for obvious reasons, from disclosure to the general public (10 C.F.R. 2.790(d)). As of the present, despite offers by the Licensee to have a properly qualified physical security expert review the plan, no such review has occurred. Moreover, none of the allegations of inadequacy have addressed the supposed shortcomings of the Physical Security Plan.

is located. None of the five examples cited by the Intervenor relate specifically to the reactor facility in Building #42, and thus are of little consequence. Indeed, the reactor facility and its controlled access area were not questioned by the Defense Audit Service (DAS) in what is still a "draft" audit report as being deficient in physical security or having been mismanaged.

The physical security protection standards required of licensees who possess special nuclear material of moderate or low strategic significance (AFRRI's is low) relate principally to detection of breaches of physical security. As is indicated in 10 C.F.R. 73.67(a)(2)(i)-(iv) a physical protection system providing for "early detection and assessment of unauthorized access or activities by an external adversary;" "early detection of removal of special nuclear material;" and notification to NRC and "appropriate response forces of its removal in order to facilitate its recovery" suffices for facilities such as AFRRI's. None of the examples cited by the Intervenor demonstrate that the regulatory standards alluded to above are not being met at AFRRI. In other words, the Intervenor has shown nothing that would indicate that the NRC's approval of AFRRI's Physical Security Plan was erroneous. Indeed, the first two of Intervenor's examples communicate the fact that mechanisms exist by which access can be "controlled" in a manner which assures AFRRI's ability to achieve early

detection. In summary, the evidence of record demonstrates clearly that the regulatory requirements have been met and thus this contention must be dismissed.

CONTENTION 8 - ACCIDENTS III

This contention deals with multiple clad failures postulated to be caused by either a power excursion or a LOCA such that sudden elevated temperatures occur in turn causing multiple clad failures followed by either an explosive zirconium-steam interaction or an explosive zirconium-air interaction, respectively, depending on the accident initiator being either a power excursion or a LOCA. The Intervenor contends that such accidents constitute "maximum credible accidents beyond the design basis of the reactor (class 9 accidents)" but that they nevertheless can be expected to occur at the APFRI reactor.

Even though the Licensee has demonstrated that multiple clad failures due to a power excursion or a LOCA are not credible elsewhere in this Motion on contentions 9. Accidents IV, and 2. Accidents II.4, and although the Licensee has also demonstrated that it is extremely unlikely that significant amounts of hydrogen can be driven out of failed TRIGA fuel contention 2. Accidents II.3, the impossibility of explosive zirconium-steam or zirconium-air interactions occurring even at elevated temperatures in TRIGA fuel must still be discussed. The Intervenors have pointed this out as their primary area of concern.

First, before beginning the discussion of zirconium-steam and zirconium-air interactions in TRIGA fuel, one very interesting point should be made. That is, the Intervenor's own wording of this contention classifies these events as "maximum credible accidents beyond the design basis of the reactor (class 9 accidents)" and yet also submits that these postulated "maximum credible accidents beyond the design basis of the reactor (class 9 accidents)" can nevertheless be expected to occur at the AFRRI reactor. Licensee is at an obvious loss in trying to resolve this clear contradiction. That is, how can accidents which are admittedly "beyond the design basis" possibly be "expected to occur"? The Intervenor seems to suffer from the same problem. When the Licensee asked the Intervenor's technical consultant, Dr. Irving Stillman, "Can accidents which are, by your own admission and contention, which are beyond the design basis, actually be expected to occur?", Dr. Stillman replied "Yes." Further, when Licensee followed up on this line of questioning during the deposition of Dr. Stillman in New York on 18 Dec 1982, by asking Dr. Stillman, "You feel that it could happen over the lifetime of the facility?", Dr. Stillman replied, "Yes, obviously." And yet when Licensee continued by asking Dr. Stillman, "Has it ever occurred at any TRIGA reactor facility that you are aware?" Dr. Stillman replied, "No, not that we know of." (See page 131 lines 9 through 25, inclusive,

of the transcript of Dr. Stillman's deposition taken in New York on 18 Dec 82.) Moreover, when the Licensee asked Dr. Stillman to put a qualitative estimate of likelihood on whether explosive zirconium-steam or zirconium-air interactions would occur at AFRI if clad failures and elevated temperatures were presumed to exist, Dr. Stillman characterized such interactions as being "Unlikely."

(See page 132 lines 1 through 24, inclusive, of the transcript of Dr. Stillman's deposition in New York on 18 Dec 1982.)

The Licensee submits that multiple clad failures alone are not credible. Even if multiple clad failures did occur, along with elevated temperatures, the explosive zirconium-steam and zirconium-air interactions are simply not possible. (See General Atomics Report #GA-A15384, "TRIGA Low-Enriched Uranium Fuel Quench Tests," by J.R. Biddlecome, et al., GA Project No. 4314, July 1980. See also, "Fuel Elements for Pulsed TRIGA Research Reactors," by M. T. Simnad, et al., Nuclear Technology, Vol 28, January 1976, pp. 31-56 at page 37. Both of these documents are contained in Attachment 7.

Very simply stated explosive zirconium-steam and zirconium-air interactions simply cannot occur for TRIGA fuel at AFRI. This statement is based on actual experiments performed by General Atomics. In one of these experiments, unclad U-ZrH_x fuel slugs were heated inductively up to 1200°C and then immediately quenched in water. No zirconium-steam interaction resulted even though hydrogen was driven

off. In fact, only minor surface slug cracking resulted. For the other experiment series, again unclad U-ZrH_x fuel slugs were inductively heated up to a temperature of 850°C and then air was introduced into the chamber. Here also, no explosive zirconium-air interaction occurred. These experiments are detailed in the reports in Attachment 7.

It is extremely noteworthy that during the deposition of Dr. Irving Stillman that when Licensee asked Dr. Stillman: "Do you have any documentation that shows that explosive zirconium-steam and/or zirconium-air interactions do or can occur for TRIGA fuel?" Dr. Stillman replied, "No, not for TRIGA fuel I don't." (See page 132 line 25 through page 133 line 3, inclusive, of the transcript of Dr. Stillman's deposition taken in New York on 18 Dec 1982.)

It is also noteworthy that the Intervenors are aware of the General Atomics experiments which refute the possibility that explosive zirconium-steam and zirconium-air interactions can occur in TRIGA fuel and yet "poo-poo" these results and try to disclaim them. (See Intervenor's response to NRC Staff request for admissions #9 where Intervenors state, "Tests of chemical reactivity (such as those described in your assertion) were performed with TRIGA fuel elements at the General Atomic Corp. Laboratories. To have complete confidence in their results would be comparable to asking the tobacco industry to determine the effect of cigarette

smoking on the incidence of lung cancer. In other words, General Atomic is the manufacturer of both the TRIGA-reactor and its fuel elements, hardly an impartial scientific study is to be expected.") On the other hand, though, when the Intervenor supplemented its responses to Licensee's first round interrogatories #35g and 35i, the Intervenor stated in both responses, "To the best of our knowledge, General Atomic Company has not attempted such experiments even though they are ideally set up to perform them." The Licensee submits that the Intervenor is, at best, confused or, at worst, will only accept that information which is agreeable to itself.

In summary, the Licensee has demonstrated elsewhere in this Motion that multiple clad failures are not credible for power excursions or a LOCA since conditions for causing such clad failures cannot be attained. Licensee has also demonstrated, elsewhere in this Motion, the extreme difficulty and, thus, extreme unlikelihood of driving significant amounts of hydrogen from failed TRIGA fuel. Moreover, the Licensee has demonstrated for its reactor that even if multiple clad failures and elevated temperatures are presumed, explosive zirconium-steam or zirconium-air interactions will not occur. The Licensee submits that the experiments performed by General Atomic on such chemical reactions serve as proof to substantiate this claim of impossibility. The Licensee submits that the Intervenor's

claims under this contention are totally without support. Therefore, based on the discussion provided herein and the results of the cited General Atomics experiments, this contention must be summarily dismissed.

CONTENTION 9 - ACCIDENTS IV

This contention deals with a presumed LOCA, where the core becomes uncovered. The Intervenor contends that if core uncovering occurs concurrently with pulsing operation, that multiple clad failures could result.

There is no real disagreement among the parties that the multiple cladding failures postulated by the Intervenor can only be achieved after elevating the fuel temperature drastically over a substantial time interval. Part of this increase in fuel temperature, according to the Intervenor's hypothesis, comes from the "loss of cooling and shielding water" (or LOCA). The other part comes from the repeated continuation of pulsing operation during the LOCA. Indeed, the scenario advanced by the Intervenor demands a LOCA coupled with repetitive pulsing. The heart of the disagreement is whether or not all of these conditions can occur simultaneously (if at all).

The Licensee has demonstrated (in Attachment 8 hereto) that numerous safeguards must fail (extremely unlikely) and gross operator error must be assumed to permit the selective and fast repetitive firing out of the transient control rod during a LOCA. However, even if this incredible

series of events and malfunctions were nevertheless presumed to occur, the Licensee has still demonstrated that actual repetitive pulses cannot occur at a frequency greater than about one every 10 seconds by which time fuel temperatures will have basically recovered to ambient conditions. The Licensee, moreover, has demonstrated that uncovered fuel regions cannot effectively contribute to fission, and, thus, fuel temperatures in such uncovered regions cannot become aggravated any further, i.e., beyond the conditions that are posed as a result of the LOCA by itself. The Licensee submits, therefore, that the Intervenor's claims under this contention are totally without support. Licensee further submits that its LOCA analyses within the AFRRI SAR together with a former LOCA analysis submitted under Docket 50-170 in 1964-65 as part of a previous license amendment are true, realistic, and, in fact, conservative and provide reasonable assurance that no clad failures are expected in conjunction with a LOCA at AFRRI.

CONTENTION 10 - ROUTINE EMISSIONS II

This contention was originally part of what is now Contention 4 - Routine Emissions I. In essence, the Intervenor alleges in Contention 10 additional examples of historical events at AFRRI that supposedly demonstrate that radiation monitoring methods are inadequate and that prior violations of regulatory limits did result from routine reactor operations.

The Intervenor argues that annual doses to the general public have previously exceeded 0.5 rem. In support of this argument, the Intervenor identifies three AFRRRI Environmental Release Data and Perimeter Monitoring Reports (dated 5/27/66, 9/20/66, and 12/14/71⁴) and an Autumn, 1979, written communication from AFRRRI to Mr. Joe Miller, a member of CNRS.

Licensee submits that there is no material issue of fact that remains as to this contention. AFRRRI's reports to the AEC/NRC speak for themselves. Properly understood, these reports demonstrate that the conclusion reached in other documents in Docket 50-170 are correct. For example, the NRC Staff concludes at section 12.9 of its SER that:

The results of the environmental radiation dosimeters (film or TLD) located on the NNMC grounds have averaged less than 3 mrems/yr for the last 10 years. The average of the highest individual readings for the last 10 years is less than 15 mrems/yr.

In addition, the NRC Staff dealt with and adequately explained (at SER Section 12.9) the significance of 1-5 mrems/hour dose rate and the high environmental monitoring station reading reported on 12/14/71 as follows:

During the 1960's AFRRRI operated an x-ray facility in support of its research program, and a nearby perimeter monitoring station consistently gave

^{4/} While this date is given as 12/14/77, Licensee suggests that 1971 is the year intended by the Intervenor.

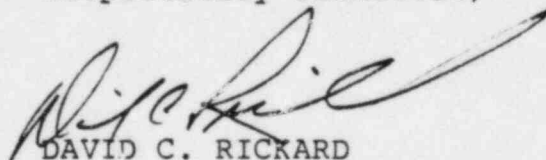
a reading much higher than any other, or the average of the others, in the perimeter monitoring set. Both because of the proximity of the x-ray lab and because there is no credible way the reactor airborne effluents could always flow toward the station, it is concluded that readings at that detector station were not related to the reactor or other NRC-licensed operations.

In short, it is clear from an examination and proper interpretation of the evidence of record that no material disagreement of fact remains and that this contention must be dismissed.

CONCLUSION

As is clear from this motion and its attachments, the vast majority of the issues before the Board in this proceeding are no longer the subject of a genuine dispute from a factual point of view. While there was clearly a great deal at issue at the time the contentions involved were admitted, the discovery process has provided data which narrows the focus of this dispute considerably. The Licensee therefore requests that the Board dismiss Contentions 1, 2, 4, 5, 6, 7, 8, 9 and 10. The Licensee further suggests that a suitable schedule for the disposition of Contention 3 be established.

Respectfully submitted,



DAVID C. RICKARD
Deputy General Counsel
Defense Nuclear Agency
Counsel for Licensee

ATTACHMENT 1

Qualifications/Resume

JOSEPH A. SHOLTIS, JR., MAJOR, USAF

Armed Forces Radiobiology Research Institute (AFRRI)
Radiation Sciences Department (RSD)
Radiation Sources Division (RSRS)
Naval Medical Command, National Capital Region
Bethesda, Maryland 20814

PROFESSIONAL CIVILIAN EDUCATION

- 1970 B.S. Nuclear Engineering, The Pennsylvania State University, University Park, PA. (Distinguished Military Graduate). GPA: 2.9
- 1977 M.S. Nuclear Engineering, The University of New Mexico, Albuquerque, NM. GPA: 4.0
- 1977 -
1980 Ph.D. Course work, Nuclear Engineering, The University of New Mexico, Albuquerque, NM. GPA: 4.0

PROFESSIONAL MILITARY EDUCATION

- 1975 USAF Squadron Officer School, Maxwell AFB, AL. (via correspondence).
- 1978 USAF Air Command and Staff College, Maxwell AFB, AL. (via non-resident seminar at Kirtland AFB, NM).

CIVILIAN & MILITARY COURSES/TRAINING

- 1968 Logic & Event Tree Analysis, California State College, California, PA.
- 1972 Advanced Nuclear Power Plant Technology, Georgia Institute of Technology, Atlanta, GA.
- 1973 Essentials of Fluid Mechanics: Statics and Dynamics of Fluid Flow, Air Force Institute of Technology, Wright-Patterson AFB, OH.
- 1973 Nuclear Weapons Effects, Air Force Institute of Technology, Wright-Patterson AFB, OH.
- 1975 USAF Laboratory Management of R&D Procurement, Kirtland AFB, NM.
- 1976 USAF Nuclear Accident Disaster Preparedness, Kirtland AFB, NM.
- 1976 Environmental Impact Statements for the DoD, General Services Administration, Dallas, TX.
- 1977 Nuclear Criticality Safety short course and laboratory work shop, University of New Mexico and Los Alamos Scientific Laboratory, Los Alamos, NM and Taos, NM.

- 1981 Medical Effects of Nuclear Weapons, Armed Forces Radiobiology Research Institute, Bethesda, MD.
- 1982 Nuclear Weapons (advanced course), Interservice Nuclear Weapons School, Kirtland AFB, NM.
- 1983 Ballistic Missile Staff Course, USAF Air University, Vandenberg AFB, CA.

PROFESSIONAL EXPERIENCE

- 1982 - Present Chief, Radiation Sources Division and Reactor Physicist-In-Charge, Armed Forces Radiobiology Research Institute, Bethesda, MD.
- 1981 - 1982 Reactor Branch Chief and Reactor Physicist-In-Charge, Armed Forces Radiobiology Research Institute, Bethesda, MD.
- 1980 - 1981 Research Reactor Operations Officer, Armed Forces Radiobiology Research Institute, Bethesda, MD.
- 1978 - 1980 USAF laboratory Associate and DoD Member of the Technical Staff, Advanced Reactor Safety Division, Sandia National Laboratories, Albuquerque, NM.
- 1974 - 1978 Chief, Space Nuclear Systems Safety Section, Air Force Weapons Laboratory, Kirtland AFB, NM.
- 1971 - 1974 Foreign Aerospace Nuclear Power Systems Analyst, Foreign Technology Division, Wright-Patterson AFB, OH.
- 1968 - 1971 Mine Safety Analyst/Statistician, U.S. Bureau of Mines, Pittsburgh, PA.

COMMITTEES, CONSULTANTSHIPS, AND SPECIFIC PROJECT EXPERIENCE

- 1971 - 1974 Identified, Evaluated and Characterized the Design Performance Capabilities of Foreign Ground and Aerospace Nuclear Power Systems, including the Soviet "Romashka" and "Topaz" Space Reactors, Foreign Technology Division, Wright-Patterson AFB, OH.
- 1971 - 1974 Briefer: "The Soviet Technological Challenge," Foreign Technology Division, Wright-Patterson AFB, OH.
- 1974 - 1976 Project Officer, Feasibility and Safety Analysis and Component Testing of Nuclear Propulsion and Power Systems for the USAF, Air Force Weapons Lab, Kirtland AFB, NM.

- 1974 -
1978 Project Officer, Nuclear Safety/Risk Assessments for the Launch of U.S. Space Nuclear Power Systems, Air Force Weapons Lab, Kirtland AFB, NM.
- 1974 -
1978 Technical Advisor, Interagency Nuclear Safety Review Panels for the the Viking A & B, Lincoln Experimental Satellites 8/9, Pioneer 10 & 11, and Voyager I & II Launches; Air Force Weapons Lab, Kirtland AFB, NM, HQ, USAF, Washington, D.C., HQ Air Force Systems Command, Andrews AFB, MD, NASA-Kennedy Space Center, and NASA-Houston Space Center.
- 1975 -
1976 Member, DoD Tri-Service Working Group on Nuclear Power for the DoD.
- 1976 -
1978 Technical Advisor, Blue Ribbon Panel on Advanced Space Power Systems for the DoD in the 1980's and Beyond, HQ, USAF, Washington, D.C.
- 1976 -
1978 Project Officer, AFSATCOM II/III Nuclear Safety/Risk Evaluation and Environmental Impact Statement, Air Force Weapons Lab, Kirtland AFB, NM.
- 1976 -
1978 Member, New Mexico Governor's Panel (New Mexico Energy Resource Registry) on Energy and Scientific Manpower Resources, Santa Fe, NM.
- 1976 -
1977 Evaluation Team Member, Procurement of Kilowatt Isotopic Power System (KIPS) for DoD/DoE/NASA Space Use, Germantown, MD.
- 1976 -
1978 Inspector, Kirtland AFB Nuclear Disaster Preparedness Inspection/Implementation Team, Kirtland AFB, NM.
- 1976 -
1978 Member, Mark 12/12A Reentry Vehicle Test Launch Search and Recovery Team, Enewetak Atoll.
- 1976 -
1978 Lead Project Officer, Safety Evaluation of the Space Shuttle Launch Vehicle, Air Force Weapons Laboratory, Kirtland AFB, NM.
- 1978 Member, Nuclear Weapons Stockpile INRAD Survey Team, Air Force Weapons Laboratory, Kirtland AFB, NM.
- 1978 -
1980 Technical Advisor, DoE/NRC Probabilistic Risk Assessment Review Group.
- 1978 -
1980 Technical Advisor, Reactor Safety Committee, Sandia National Laboratories, Albuquerque, NM.

- 1978 -
1980 Principal Investigator, Accident Initiation and Engineered Safety Systems: LMFBR Accident Delineation Study, Sandia National laboratories, Albuquerque, NM.
- 1978 -
1980 Project Officer, Characterization of Sandia Lab's Annular Core Research Reactor (ACRR) Performance Characteristics, Sandia National Laboratories, Albuquerque, NM.
- 1978 -
1980 Project Officer, Evaluation of LMFBR Transient Overpower (TOP) Accidents and Their Initiators & Proposal of In-Pile Experimentation to Study TOP Accident Progression and Phenomenology, Sandia National Laboratories, Albuquerque, NM.
- 1979 -
1980 Lecturer, The Three-Mile Island Unit 2 Accident, Albuquerque, NM.
- 1981 -
Present Member, AFRRI Reactor and Radiation Facility Safety Committee, Armed Forces Radiobiology Research Institute, Bethesda, MD.
- 1981 -
Present Instructor: "Principles of Ionizing Radiation" and "Electromagnetic Pulse" units of Medical Effects of Nuclear Weapons Course, Armed Forces Radiobiology Research Institute, Bethesda, MD.
- 1981 Member, Cobalt-60 Recovery Team: Project HERMAN, Armed Forces Radiobiology Research Institute, Bethesda, MD.
- 1983 -
Present Invited Lecturer: "Principles of Ionizing Radiation" unit of Military Applied Physiology Course, Uniformed Services University of the Health Sciences, Naval Medical Command, National Capital Region, Bethesda, MD.

CERTIFICATION

- 1980 USNRC Reactor Operator, License No. OP5363
- 1981 USNRC Senior Reactor Operator, License No. SOP3942

PROFESSIONAL AFFILIATIONS

- Member: American Nuclear Society (ANS)
- Member: TRIGA Reactor Owners/Users/Operators Group
- Member: Test, Research, and Training Reactors (TRTR) Organization
- Member: American Association for the Advancement of Science (AAAS)
- Member: New York Academy of Sciences (NYAS)
- Member: The Planetary Society

Member: Americans for Rational Energy Alternatives (AREA)

Member: Scientists and Engineers for Safe Secure Energy (SE-2)

Member: Society of American Military Engineers (SAME)

Associate

Member (Nominee) American Society of Mechanical Engineers (ASME)

PUBLICATIONS

Sholtis J A, Jr. "The Dissociating Gas Power Cycle (U)," Foreign Technology Division Bulletin, Foreign Technology Division, Wright-Patterson AFB, OH, 16 Apr 1974, (SECRET/NOFORN).

Sholtis J A, JR. Title Classified, Foreign Technology Division Bulletin, TCS-384491/74, SAO/FTD-SP-13-01/06-74, Foreign Technology Division, Wright-Patterson AFB, OH, 14 May 1974, (TOP SECRET).

Sholtis J A, JR. "Soviet Aerospace Nuclear Reactor Technology (U)," Contribution to Defense Intelligence Agency Task T70-02-01B, "Soviet Nuclear Power Technology (U)," Foreign Technology Division, Wright-Patterson AFB, OH, 31 Oct 1972, (SECRET/NFD).

Sholtis J A, Jr. "Aerospace Nuclear Reactor Technology - Western Europe (U)," Contribution to Defense Intelligence Agency Task T74-02-09, "Nuclear Power Technology - Western Europe (U)," Foreign Technology Division, Wright-Patterson AFB, OH, 31 Oct 1973 (SECRET/NFD/NDA).

Sholtis J A, Jr. "Radial and Axial Neutron Flux Profiling for Small Heterogeneous Reactor Cores by Redistribution of Fuel," AFWL-TR-75-246, Air Force Weapons Laboratory, Kirtland AFB, NM, Mar 1976.

Sholtis J A, Jr. "Empirical Correlation Describing the Impact Response of Two-Foot Diameter Spheres with Internal Energy Absorbing Material Simulating an Airborne Reactor Containment System," AFWL-TR-76-93 (Rev.), Air Force Weapons Laboratory, Kirtland AFB, NM Aug 1976.

Sholtis J A, Jr. "Description and Analysis of Kilowatt Isotope Power Systems (KIPS) Under Development for Space Application in the 1980's," AFWL-TR-76-207, Air Force Weapons Laboratory, Kirtland AFB, NM, Feb 1977.

Holtzscheiter E W, Kelleher D, Mitchell G, Crawford M L, and Sholtis J A, Jr. "Safety Methodology for Space Nuclear Systems," AFWL-TR-77-104, Air Force Weapons Laboratory, Kirtland AFB, NM, Oct 1977.

Sholtis J A, Jr. "Preliminary Safety and Environmental Assessment (PSEA) of a Nuclear-Powered Strategic Satellite System (SSS)," Internal Air Force Weapons Laboratory/DYVS Report, Air Force Weapons Laboratory/DYVS, Kirtland AFB, NM, 1 Jul 1978.

Sholtis J A, Jr. "Synchronous Satellite/Spacecraft Collision Probabilities," Internal Air Force Weapons Laboratory/NSO Technical Report, Air Force Weapons Laboratory/NSO, Kirtland AFB, NM, 8 Oct 1976.

- Sholtis J A, Jr. "Economic Impact to the U.S. of No Breeder Reactor Program (LMFBR) and No Reprocessing of Spent Nuclear Fuel Over the next Thirty Years," paper presented at the First DoE Nuclear Data Conference, Albuquerque, NM, Oct 1977.
- Sholtis J A, Jr. "Environmental Impact Statements for U.S. DoD Space Nuclear Systems," USAF Nuclear Surety Information, Vol 12, No. 45, Jul-Sep 1978.
- Sholtis J A, Jr. and Crawford M L. "Preorbital Risk Assessments for the Launch of U.S. Space Nuclear Systems," Internal Air Force Weapons Laboratory/DYVS Report, Air Force Weapons Lab/DYVS, Kirtland AFB, NM, Oct 1978.
- Sholtis J A, Jr. "Simulated Response of an Airborne Reactor Containment System to Impact," paper presented at the American Nuclear Society 1977 Western Regional Student Conference, 23-2 Mar 1977, Oregon State University, Corvallis, OR.
- Sholtis J A, Jr. "Mission, Design, and Safety Considerations of Aircraft Nuclear Propulsion for the DoD," Independent M.S. Study Report, University of New Mexico, Department of Chemical and Nuclear Engineering, Albuquerque, NM, Jun 1977.
- Sholtis J A, Jr. "Impact Testing and Analysis of Airborne Reactor Containment Vessels," paper presented at the University of New Mexico, Chemical and Nuclear Engineering Seminar, Albuquerque, NM, 22 Mar 1977.
- Williams D C, Sholtis J A, Jr., Rios M, Varela D W, Worledge D H, Conrad P W, and Pickard P S. "LMFBR Accident Delineation Study: Approach and Preliminary Results," Sandia National Laboratories, Albuquerque, NM, paper presented at the ANS/ENS International Meeting on First Reactor Safety Technology, Seattle, WA, 19-23 Aug 79.
- Varela D W, Sholtis J A, Jr., and Worledge D H. "Justification for Low-Ramp Transient Overpower (TOP) Experiments," Sandia National Laboratories Technical Report to J.E. Powell and the Nuclear Regulatory Commission Office of Advanced Reactor Safety Research, Sandia National Laboratories, Albuquerque, NM, 26 Mar 1979.
- Williams D C, Varela D W, Worledge D H, and Sholtis J A, Jr. "Delineation of LMFBR In-Core Accident Phenomenology," SAND79-0113A, Sandia National Laboratories, Albuquerque, NM, Aug 1979.
- Sholtis J A, Jr., Rios M, Worledge D H, Conrad P W, Williams D C, Varela D W, and Pickard P S, "LMFBR Accident Delineation Study, FY 79 Interim Report," SAND79-0100A, Sandia National Laboratories, Albuquerque, NM, Aug 1979.
- Sholtis J A, Jr. "Analysis of the COSMOS 954 Reentry (U)," Internal Air Force Weapons Laboratory/NSCM Report, Air Force Weapons Lab/NSCM, Kirtland AFB, NM, (Confidential).
- Rios M, Sholtis J A, Jr., Williams D C, Conrad P W, and Pickard P S. "LMFBR Accident Delineation Study, Phase IA Final Report, Sandia National Laboratories, Albuquerque, NM, 1 Oct 1978.
- Williams D C, Sholtis J A, Jr., Conrad P W, and Pickard P S. "LMFBR Accident Delineation Study, Phase I Final Report," NUREG/CR-1507, SAND80-1267, Sandia National Laboratories, Albuquerque, NM, 15 Nov 1980.

- Sholtis J A, Jr. "LMFBR Accident Delineation: Development of the Methodology and its Application to Transient Overpower (TOP) Accidents, SAND80-1413, NUREG/CR-1550, Sandia National Laboratories, Albuquerque, NM, 1980.
- Williams D C, Sholtis J A, Jr., and Sciacca F W. "LMFBR Accident Delineation and the Evaluation of Research Priorities," SAND80-1634A, TANSO-35-1-676 (1980), Vol 35, Sandia National Laboratories, Albuquerque, NM, paper presented at the 1980 ANS/ENS International Conference on Fast Reactor Safety, 16-21 Nov 1980, Washington, D.C.
- Sciacca F W, Sholtis J A, Jr., and Williams D C, "LMFBR Accident Delineation Study: Assessment of Post-Accident Phenomenology," TANSO-35-1-676 (1980), Vol 35, ISSN: 0003-018X, pp. 386-387, Sandia National Laboratories, Albuquerque, NM, paper presented at the 1980 ANS/ENS International Conference on Fast Reactor Safety, 16-21 Nov 1980, Washington, D.C.
- Sholtis J A, Jr. "Nuclear Criticality Safety Analysis of Hypothetical AFRRI-TRIGA Fuel Element Storage Rack Accidents," Internal Armed Forces Radiobiology Research Institute/SSD Memorandum for Record, Armed Forces Radiobiology Research Institute/SSD, Bethesda, MD, 19 Jan 1981.
- Sholtis J A, Jr. and Moore M L. "Reactor Facility, Armed Forces Radiobiology Research Institute," AFRRI-TR81-2, Armed Forces Radiobiology Research Institute, Bethesda, MD, May 1981.
- Sholtis J A, Jr. "Analysis of Cocked Fuel Elements in the AFRRI-TRIGA Mark-F Reactor," Armed Forces Radiobiology Research Institute, Bethesda, MD, paper presented at the 8th TRIGA User's Conference, 8-10 Mar 1982, Idaho Falls, ID.
- Moore M L and Sholtis J A, Jr. "AFRRI Reactor Relicensing Effort," Armed Forces Radiobiology Research Institute, Bethesda, MD, paper presented at the 8th TRIGA User's Conference, 8-10 Mar 82, Idaho Falls, ID.
- Sholtis J A, Jr. "Analysis of the Consequences of a Hypothetical Worst-Case Reactivity Excursion (i.e., Inadvertent Pulse) While Operating the AFRRI-TRIGA Reactor in the Steady-State Mode at Full Power (1.0 Mwt)," Internal Armed Forces Radiobiology Research Institute/SSD Memorandum for Record, Armed Forces Radiobiology Research Institute/SSD, Bethesda, MD, 5 Feb 82.
- Sholtis J A, Jr. "Xenon Buildup and Associated Negative Reactivity Worth Determinations over Time during AFRRI Reactor Power Operations as well as after Scram," Internal Armed Forces Radiobiology Research Institute/RSRS-Reactor Technical/Operational Data Report, Armed Forces Radiobiology Research Institute/RSRS-Reactor, Bethesda, MD, 1982.
- Sholtis J A, Jr. "AFRRI Emergency Evacuation and Fire Plan," Armed Forces Radiobiology Research Institute Instruction 3020.2G, Armed Forces Radiobiology Research Institute, Bethesda, MD, 30 Sep 82.
- Sholtis J A, Jr. Responses to NRC Staff Questions concerning the "AFRRI Reactor Facility Safety Analysis Report (SAR)," Facility License R-84, Docket No. 50-170, incorporated as an addendum to the AFRRI SAR dated Jun 81, Bethesda, MD, 9 Oct 81.

Sholtis J A, Jr. "Emergency Plan for the AFRRI-TRIGA Reactor Facility," License R-84, Docket No. 50-170, Armed Forces Radiobiology Research Institute, Bethesda, MD, Oct 1982.

Smoker R R and Sholtis J A, Jr. "AFRRI Radiation Sources Division Instructions RSD 5-1 through 5-9 inclusive," Armed Forces Radiobiology Research Institute/RSRS, Bethesda, MD, dates on individual RSD Instructions vary from 1980 through 1981.

Numerous responses to intervenor (CNRS) interrogatories under the contested AFRRI Reactor license renewal proceeding before USNRC.

Numerous one-time and recurrent monthly, quarterly, and annual technical progress reports and project status reports over the last twelve years.

HONORS, AWARDS, AND ACCOLADES

- 1965 Elected to National Honor Society
- 1965 Selected for Washington County, PA, Gifted Student Program at California State College, California, PA
- 1966 Honor Graduate, Monongahela High School, Monongahela, PA
- 1969 Vice-Commandant's Award, AFROTC Field Training, Plattsburgh AFB, NY
- 1970 Commissioned 2Lt, USAF, Distinguished Military Graduate, The Pennsylvania State University, University Park, PA
- 1971 National Defense Service Medal
- 1972 Junior Officer of the Quarter, Foreign Technology Division, Wright-Patterson AFB, OH
- 1972 Air Force Systems Command, Certificate of Merit
- 1974 Air Force Commendation Medal
- 1975 USAF Outstanding Unit Award
- 1976 Air Force Commendation Medal, 1st Oak Leaf Cluster
- 1977 USAF Outstanding Unit Award, 1st Oak Leaf Cluster
- 1977 American Nuclear Society, Conference Session Best Paper Award
- 1978 USAF Certificate of Appreciation
- 1978 Tendered Regular Commission, USAF
- 1980 Air Force Commendation Medal, 2nd Oak Leaf Cluster
- 1980 Sandia National Laboratories, Honorary Staff Award
- 1981 US Army Reactor Shift Superintendent's Badge

- 1981 Defense Nuclear Agency, Certificate of Achievement
- 1983 US Army Reactor Commander's Badge
- 1983 Charter Nominee to First (1983) Edition of "Who's Who in Frontier Science and Technology"

SECURITY CLEARANCES

1971 -
Present Top Secret (DoD)

1978 -
Present O-clearance (DoE)

RELEVANT EXPERIENCE SUMMARY

Twelve years experience in the design, evaluation, characterization, analysis, safety, development, procurement, use, operation, and risk assessment of thermal and fast nuclear reactor systems and radioisotopic power systems for both ground and aerospace applications.

Two years experience planning, coordinating, conducting, and assessing in-pile reactor experiments in support of the U.S. Advanced Reactor Development and Safety Analysis Program administered by USNRC.

Seven years experience with the design, safety, operation, maintenance evaluation, administration, and use of research reactors; five years of which specifically involved TRIGA Reactors.

USNRC-Licensed Senior Reactor Operator and Physicist-in-Charge for the AFRRRI TRIGA Reactor Facility.

Twelve years active commissioned service as a Nuclear Research Officer, USAF.

Nine years managerial/supervisory experience involving technical nuclear projects and personnel.

Chief, Radiation Sources Division at AFRRRI with direct control over five radiation source facilities and nine technical staff personnel.

Three years direct experience and participation on established TRIGA Reactor Facility Safety Committees.

ATTACHMENT 2

AFFIDAVIT
OF
JOSEPH A. SHOLTIS, JR.

Joseph A. Sholtis, Jr., being duly sworn according to law, deposes and says:

The Intervenor's contention centers around the fuel temperature assumed to exist and utilized by the Licensee in analyzing this clad failure accident DBA.

The Licensee clearly states in its SAR, page 6-12, last paragraph that, "Although the measured amount of radioactive noble gases for the operating conditions in the AFRRRI reactor fuel would indicate a gap activity percentage of less than 0.01 percent, the theoretical limit of 0.1 percent gap activity for fission product gases of noble gases and iodines, as stated in reference 2, will be used in the consequence analysis for the Design Basis Accidents (Section 6.3.4)." (See also attached affidavit of Mr. Frederic D. Anderson.) Reference 2 cited above is a General Atomic Company Report titled, The U-ZrH_xAlloy: Its Properties and Use in TRIGA Fuel, by M.T. Simnad, dated February 1980, and characterized as GA Project No. 4314, GA Report No. E-117-833. A copy of Figure 5-1 from page 5-3 of this GA Report is provided below and graphically shows that a fractional release of 0.1% (or 10^{-3}), which Licensee uses in its analysis of this DBA, is associated with a fuel temperature of approximately 600°C, if the theoretical maximum curve is utilized, or a fuel temperature of approximately 800-1000°C, if the actual experimental data points are utilized. This figure proves that the Licensee did not assume in its analysis of the fuel element clad failure accident DBA that clad failure would occur at a fuel temperature of "less than 100°C" as the Intervenors contend. Instead, it shows that the Licensee utilizes a conservative release fraction associated with a minimum fuel temperature of approximately

600°C. In fact, Dr. Irving Stillman during his deposition on 18 Dec 82 tied the 0.1% release fraction that the Licensee uses to a fuel temperature of approximately 500-600°C. (See Transcript of Deposition of Dr. Irving Stillman on 18 Dec 82, page 71 line 8 through page 72 line 3.)

This is borne out in Licensee's answer to Intervenor's first-round interrogatory number 1 as well as in Licensee's answers to NRC Staff's questions on the AFRRI SAR, specifically Licensee's response to NRC Staff question #67 concerning the AFRRI SAR. It should be noted that these NRC Staff questions on the AFRRI SAR together with the responses provided have been incorporated into the AFRRI SAR as an attachment or addendum.

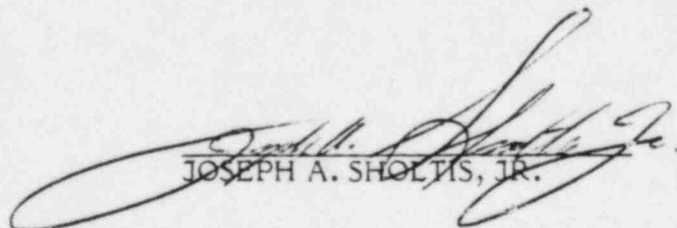
It should also be pointed out that the release fraction for accident conditions is associated with the normal operating temperature, not the temperature during accident conditions. This is because the fission products released as a result of a fuel clad failure are those that have collected in the fuel-clad gap during normal operation. (See last paragraph, labeled 3., on page 5-4 of GA Report E-117-833, a copy of which is provided below.)

Therefore, the Licensee uses in its analysis of the fuel element clad failure accident DBA a release fraction of 0.1% which is associated with a fuel temperature greater than or approximately equal to 600°C. Moreover, the 0.1% release fraction utilized by the Licensee is conservative since it is characteristic of a normal operating fuel temperature greater than or approximately equal to 600°C -- fuel temperatures at which the Licensee's reactor does not normally operate.

Intervenor has stated in its answers to Licensee's first-round interrogatories as well as during the deposition of Dr. Irving Stillman that they (the Intervenor) base their contention

statement that the Licensee utilizes a fuel temperature of "less than 100°C" in its analysis of the fuel element clad failure accident DBA on the 0.2% radioiodine release fraction cited in Licensee's SAR. Here also the Intervenor has misinterpreted the facts of the matter since the Intervenor believes that only 0.2% of the radioiodines contained in the gap get out of the element for a clad failure event. (See Transcript of Deposition of Dr. Irving Stillman on 18 Dec 1982 on page 72 lines 7 through 23 inclusive.) In fact, however, what the Licensee actually assumes is that all of the radioiodine contained in the gap at the time of a clad failure gets out and into the reactor pool water, which has a bulk temperature of approximately 25-30°C, while conservatively 0.2% of the radioiodine that gets into the reactor pool water is assumed to come out of solution and gets into the reactor room air. This point is quite clearly stated in the AFRRRI SAR on page 6-13, last paragraph, as well as in Licensee's response to NRC staff's question #43 on the AFRRRI SAR.

In summary, therefore, on both counts the Intervenor is incorrect and no evidence exists to indicate that Licensee uses a fuel temperature of "less than 100°C" in analyzing the fuel element clad failure accident DBA. On the contrary, Licensee utilizes a release fraction which is associated with a fuel temperature greater than or approximately equal to 600°C and which has been shown by actual experiments to be conservative.


JOSEPH A. SHOLTIS, JR.

Sworn to and subscribed before
me on this 25th day of Feb, 1983.

my commission expires:
April 8, 1984

Miriam G. Lealby
Notary Public

Affidavit of Fredric D. Anderson

I, Fredric D. Anderson, being duly sworn, do state as follows:

1. I was employed as a nuclear safety consultant to Dames & Moore to perform the safety analysis for the Armed Forces Radiobiology Research Institute's TRIGA research reactor (Docket No. 50-170). I am currently employed by the Nuclear Regulatory Commission as a Senior Reactor Engineer in the Division of Licensing.
2. This affidavit addresses the contention that the source term used in the safety analysis for the design basis accident of a fuel element cladding failure was non-conservative.
3. I have reviewed Section 6.0, "Safety Analysis", of the AFRRRI Reactor Facility Safety Analysis Report in its entirety and, subject to the supplemental information which follows for clarification, do hereby adopt the portion discussing fuel element cladding failure (Section 6.3.2) as true and correct to the best of my knowledge and belief. To respond to the above-stated contention, additional information is presented in my affidavit.

Source Term Used in AFRRRI Safety Analysis of Fuel Element Cladding Failure

As stated in the Safety Analysis (Section 6.3.2), I used a source term of 0.1 percent of the steady-state fission product inventory for the noble gases and radioiodines present in the gap of a fuel element. This source term would be available for release in the event of a fuel element cladding failure.

The value of this source term for gaseous fission products was selected on the basis of General Atomics experimental data and theoretical analysis given in GA Report No. E-117-833 (GA Project No. 4314) for various fuel temperatures and irradiation times. For the AFRRRI reactor operating conditions of a fuel temperature of 600°C and assuming infinite operation, a fractional release from the fuel material to the fuel element gap of 0.1 percent of the gaseous fission products was theoretically possible. From a 1966 experiment under the AFRRRI reactor operating conditions, a measured value of 0.01 percent of the gaseous fission products was obtained for the release fraction to the fuel element gap. Therefore, the source term used in the safety analysis for a fuel element cladding failure (0.1 percent) is a factor of 10 more conservative than the measured value (0.01 percent). In order for the assumed source term of 0.1 percent for a fuel element cladding failure accident in the AFRRRI research reactor to be consistent with the GA experimental data, the operating fuel temperature would have to be 1000°C or a margin of 400°C from actual operating fuel temperatures.

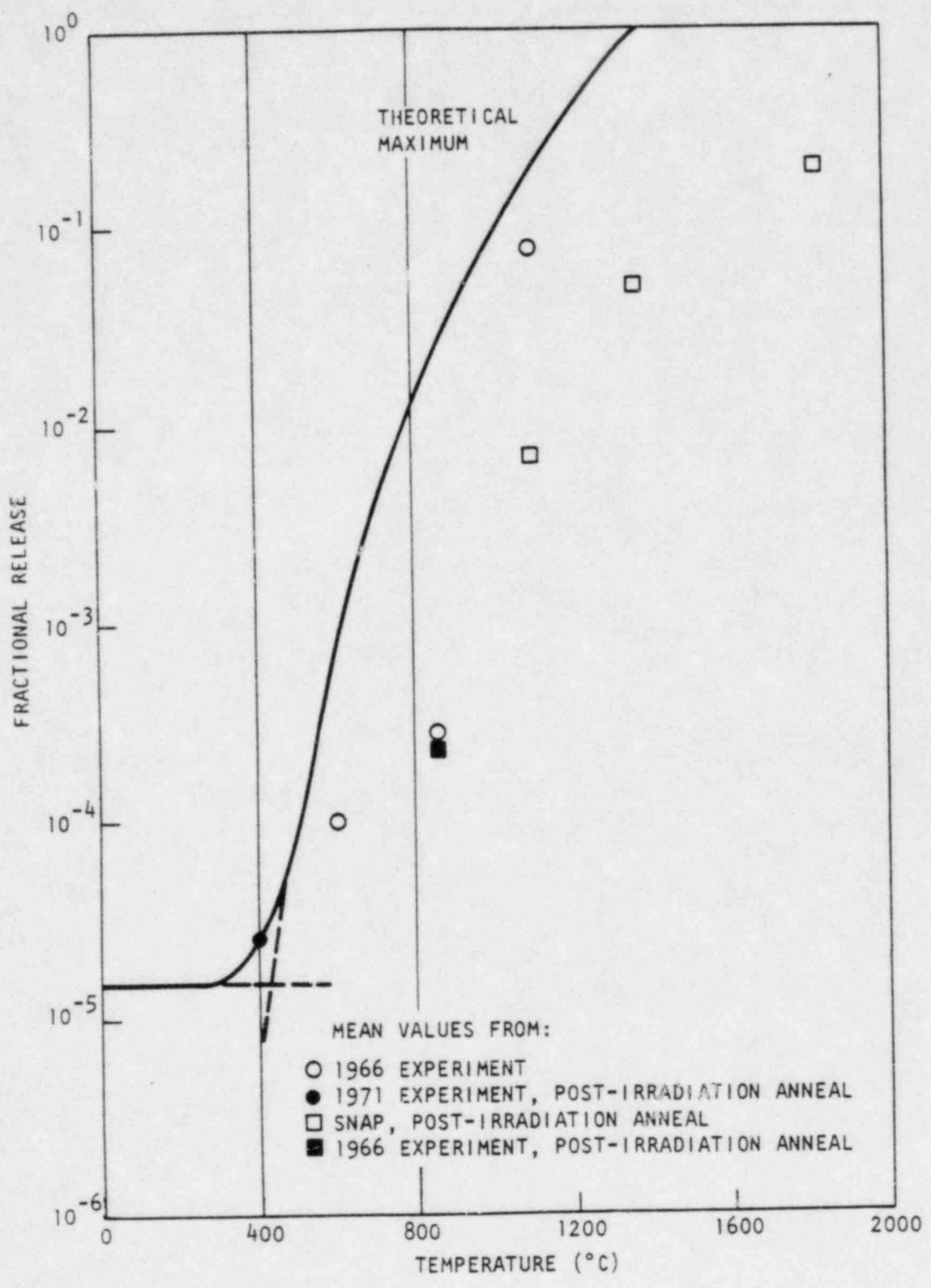
Based on the above analyses and data, I firmly believe that the source term for gaseous fission products used in the AFRRRI safety analysis for a fuel element cladding failure is appropriate and conservative.

Fredric D. Anderson
Fredric D. Anderson

Subscribed and sworn to before
me this 2nd day of December, 1982.

Judy L. Butts
Notary Public

My Commission expires July 1, 1986.



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EL-1615

Fig. 5-1. Fractional release of gaseous fission products from TRIGA fuel showing theoretical maximum, and experimental values above 400°C corrected to infinite irradiation (from Ref. 10)

The curve in Fig. 5-1 applies to a fuel element which has been irradiated for a time sufficiently long that all fission product activity is at equilibrium and the release fraction is at its theoretical maximum. Figure 5-1 shows that the measured values of fractional releases fall well below the curve. Therefore, for safety considerations, this curve gives very conservative values for the high-temperature release from TRIGA fuel.

Also worthy of note are the following conclusions from the TRIGA fission product release experiments:

1. Because the samples were unclad, the high-temperature measurements were made on essentially dehydrated U-Zr. Post-irradiation annealing measurements indicate that the dehydrating process did not significantly affect the release rate.
2. Part of the 1971 experiments was the measurement of the release from a post-irradiation anneal of a sample of fuel that had been irradiated to a burnup of ~5.5% of the U-235 (or 1.1% of the total uranium atoms). The results of this part of the experiment indicated that the effects of long-term irradiation of the fuel on fission product release are small, at least for total burnup equivalent of the maximum that has been achieved.
3. The release fraction for accident conditions is characteristic of the normal operating temperature, not the temperature during accident conditions. This is because the fission products released as a result of a fuel clad failure are those that have collected in the fuel-clad gap during normal operation.

ATTACHMENT 3

AFFIDAVIT
OF
JOSEPH A. SHOLTIS, JR.

Joseph A. Sholtis, Jr., being duly sworn according to law, deposes and says:

In a prior Memorandum for Record dated 19 January 1981, I presented conservative criticality calculations for a hypothetical AFRRRI fuel storage rack accident that prove that a twelve element configuration of stainless-steel clad TRIGA fuel elements cannot achieve criticality under any circumstances. (A copy of this 19 Jan 81 AFRRRI Memorandum for Record is provided as an attachment to this affidavit and was previously provided in response to Intervenor's Interrogatory Number 2.) In fact, Intervenor has stated in its supplemental response to Licensee's first-round Interrogatory #9e that the calculations provided in Licensee's 19 Jan 81 Memorandum do (in Intervenor's view) provide reasonable assurance that twelve stainless-steel clad TRIGA fuel elements cannot achieve criticality under any circumstances.

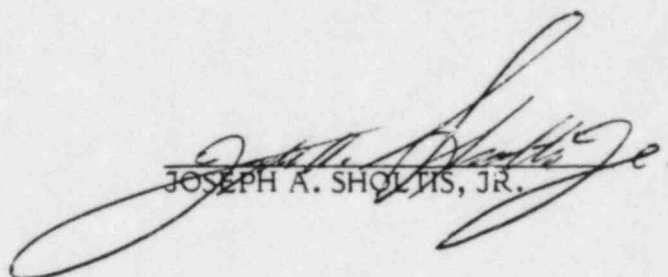
This same 19 Jan 81 Memorandum also cited a source of experience for Licensee's statement that it takes approximately 69 stainless-steel clad TRIGA fuel elements to achieve criticality. A source supporting Licensee's statement of experience that it takes approximately 69 stainless-steel clad TRIGA fuel elements to achieve criticality was also referenced in Licensee's answer to Intervenor's first-round interrogatory #3; a copy of this response is also attached to this affidavit together with a copy of AFRRRI's internal Radiation Sources Division Instruction, RSD 5-8, "Reactor Core Loading and Unloading Procedures," dated 27 March 1981. In summary, these documents identify and illustrate the actual results of the initial stainless-steel clad TRIGA fuel element core loading at AFRRRI in 1965, using the standard 1/M approach-to-critical loading technique.

Licensee is at a loss in trying to understand why Intervenor contends that a fuel element storage rack accident, that is assumed to result in a criticality excursion, can be considered an accident of a "different kind" since criticality excursions are explicitly treated in the AFRRI SAR in sections 6.2.2, 6.2.3, and 6.2.5.

Licensee is also at a loss in trying to understand how a fuel element storage rack accident that is assumed to result in a criticality excursion, regardless of its credibility or likelihood, can constitute an accident with an associated "greater severity" than accidents, like reactor power transients and clad failures, which are treated in the AFRRI SAR. This is because such an event, incredible as it appears, would occur at the bottom of the reactor pool under approximately 19.5 feet of water where adequate shielding is provided. In addition, any associated inadvertent power excursion or transient would be automatically terminated by the negative temperature coefficient of reactivity together with expansion action which would render the initially critical configuration, which is unrestrained, permanently subcritical. That is, the pool was designed to accommodate criticality, regardless of whether it occurs normally in the core or in an unplanned critical configuration of unrestrained TRIGA fuel elements, and the same intrinsic mechanisms that terminate a planned pulse would also act to terminate an unplanned excursion, except that, in addition, for the unrestrained configuration, expansion of the system during the excursion would also tend to move the elements away from one another and thus render the resultant configuration permanently subcritical. Therefore, there would be no deleterious consequences of such an event -- even if it were presumed to somehow occur.

In summary, Licensee 1) has provided conservative calculations which Intervenor even accepts as adequate assurance that a twelve element configuration of stainless-steel clad TRIGA fuel elements cannot achieve criticality, 2) has provided actual historical data for an

AFRRI core loading to support its claim that it takes approximately 69 stainless-steel clad TRIGA fuel elements to achieve criticality, 3) has, in its 19 Jan 81 Memorandum, implicitly provided an indication of the extreme difficulty required, and thus the extreme unlikelihood, of establishing an unrestrained critical or supercritical configuration of stainless-steel clad TRIGA fuel elements in the reactor pool, and 4) submits reasons and justification why a fuel element storage rack accident, that somehow manages to result in a criticality excursion at the bottom of the reactor pool, would have no deleterious consequences to the staff or the public.



JOSEPH A. SHOLTIS, JR.

Sworn to and subscribed before
me on this 25th day of Feb, 1983.

*My commission expires
April 8, 1984*

*Miriam E. Lesley
Notary Public*

file - reactor
FE

SCIENTIFIC SUPPORT DEPARTMENT

MEMORANDUM FOR RECORD:

19 January 1981

SUBJECT: Nuclear Criticality Safety Analysis of Hypothetical AFRRI TRIGA Fuel Element Storage Rack Accidents

1. An analysis was performed to substantiate that a criticality excursion would not result in the unlikely event that a fully-loaded AFRRI fuel element storage rack were to fail.
2. For the purposes of analysis, it is conservatively assumed that when the storage rack fails, all twelve fuel elements contained in the rack escape and fall to the bottom of the pool. In addition, it is conservatively assumed that the twelve fuel elements come to rest at the bottom of the pool in the most reactive neutronic configuration possible. Moreover, it is conservatively assumed that the optimum configuration of fuel elements at the bottom of the reactor tank is fully reflected by water over a complete solid angle of 4π steradians even though only 2π steradian water reflection would actually exist.
3. Fuel elements used in the AFRRI reactor are standard stainless-steel clad TRIGA elements containing U-ZrH_{1.7} with 8.5 weight percent uranium at a nominal U²³⁵ enrichment of 20 percent (See Figure 1). Each fuel element contains a nominal maximum 38 grams of U²³⁵.
4. Figure 2, reproduced from TID-7028 ⁽¹⁾, is based on experimental and analytical data and indicates that the minimum critical mass, $m_{crit.}$, for a heterogeneous, 20% enriched, fully water reflected U²³⁵ system in its most reactive configuration, * is 1.1 kg of U²³⁵. Since our assumed twelve element configuration contains a total of (12 fuel elements) X (38 grams U²³⁵/fuel element) = 456 grams U²³⁵, it would have a mass fraction critical, $m/m_{crit.}$, less than or equal to $0.456 \text{ kg U}^{235} / 1.1 \text{ kg U}^{235}$ or 0.415.

* For our assumed system, this conservative assumption not only takes into consideration an optimum reactive geometry but also neglects parasitic neutron capture in the stainless-steel clad, Sm-Al burnable poison wafers, etc. and assumes that the graphite end reflectors are replaced by water - a more effective neutron reflector.

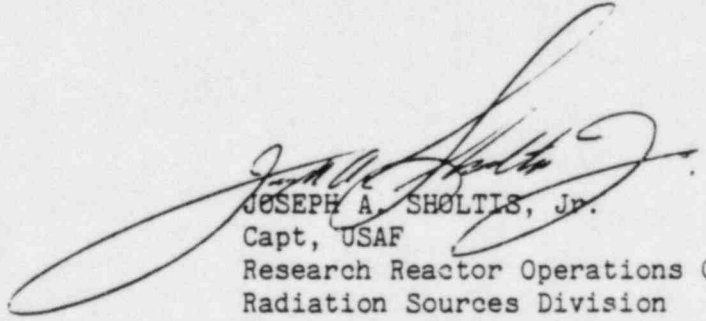
Using $k_{\text{eff}} = \sqrt[3]{m/m_{\text{crit.}}}$, (2)
indicates that our assumed system would have a $k_{\text{eff}} \leq 0.746$. Therefore, even with the application of the most conservative assumptions, our assumed system would still not achieve criticality. In fact, if our assumed system had a $k_{\text{eff}} = 0.746$, then it would be subcritical by more than $\beta_{\text{eff}} = 0.007$ (assumes $\beta_{\text{eff}} = 0.007$).

Based on the minimum critical mass, $m_{\text{crit.}}$, value of 1.1 kg U^{235} obtained from Figure 2, and a U^{235} fuel loading per element of 38 gm U^{235} , a minimum of 29 AFRRI TRIGA fuel elements arranged in an optimum neutronic configuration would be required for a criticality excursion (\approx \$.09) to occur.

5. Verification of the conservatism of this analysis is provided by data in RSD 5-8⁽³⁾. That is, experience has shown that, during actual AFRRI core loading, \sim 69 stainless-steel TRIGA fuel elements (\sim 2630 grams U^{235}) are required to achieve criticality. Therefore, since the AFRRI core lattice arrangement is very close to the optimal neutronic geometry for TRIGA fuel elements, the results of this criticality analysis are conservative by a factor of \sim 2.4 on a fuel element as well as a U^{235} mass basis for criticality.

6. In summary, a hypothetical AFRRI fuel element storage rack failure is analyzed from a nuclear criticality safety standpoint. Conservative assumptions are applied wherever possible; yet k_{eff} and $m/m_{\text{crit.}}$ for the system are found to be no greater than 0.746 and 0.415, respectively. As a result, there is no possibility of a criticality excursion in the unlikely event that a fully-loaded fuel storage rack were to fail in the AFRRI TRIGA reactor facility.

- 3 Encls
1. Fig. 1
2. Fig. 2
3. References



JOSEPH A. SHOLTIS, Jr.
Capt, USAF
Research Reactor Operations Officer
Radiation Sources Division
Scientific Support Department

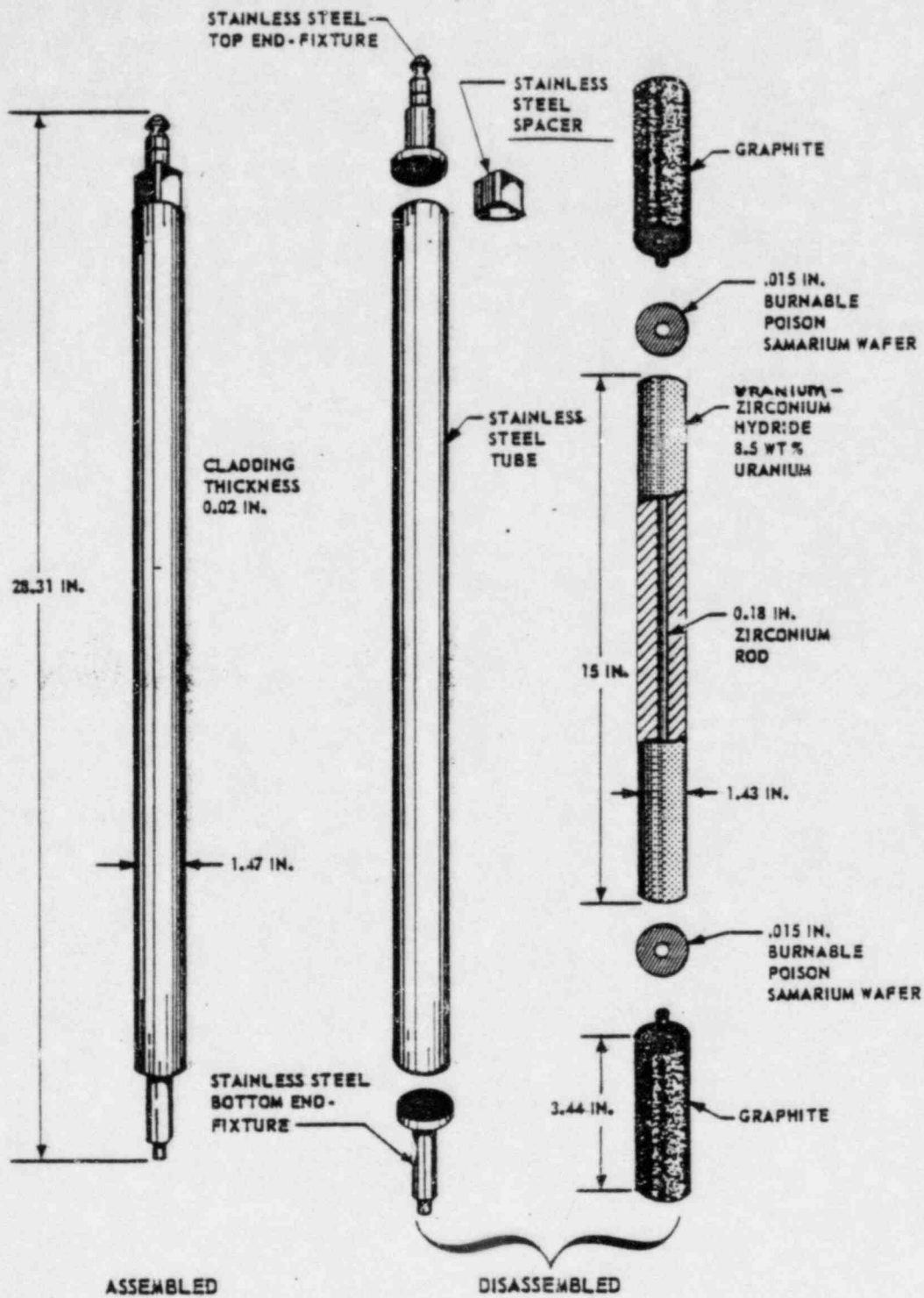


Figure 1. Standard AFRR I TRIGA Fuel Element

Encl 1

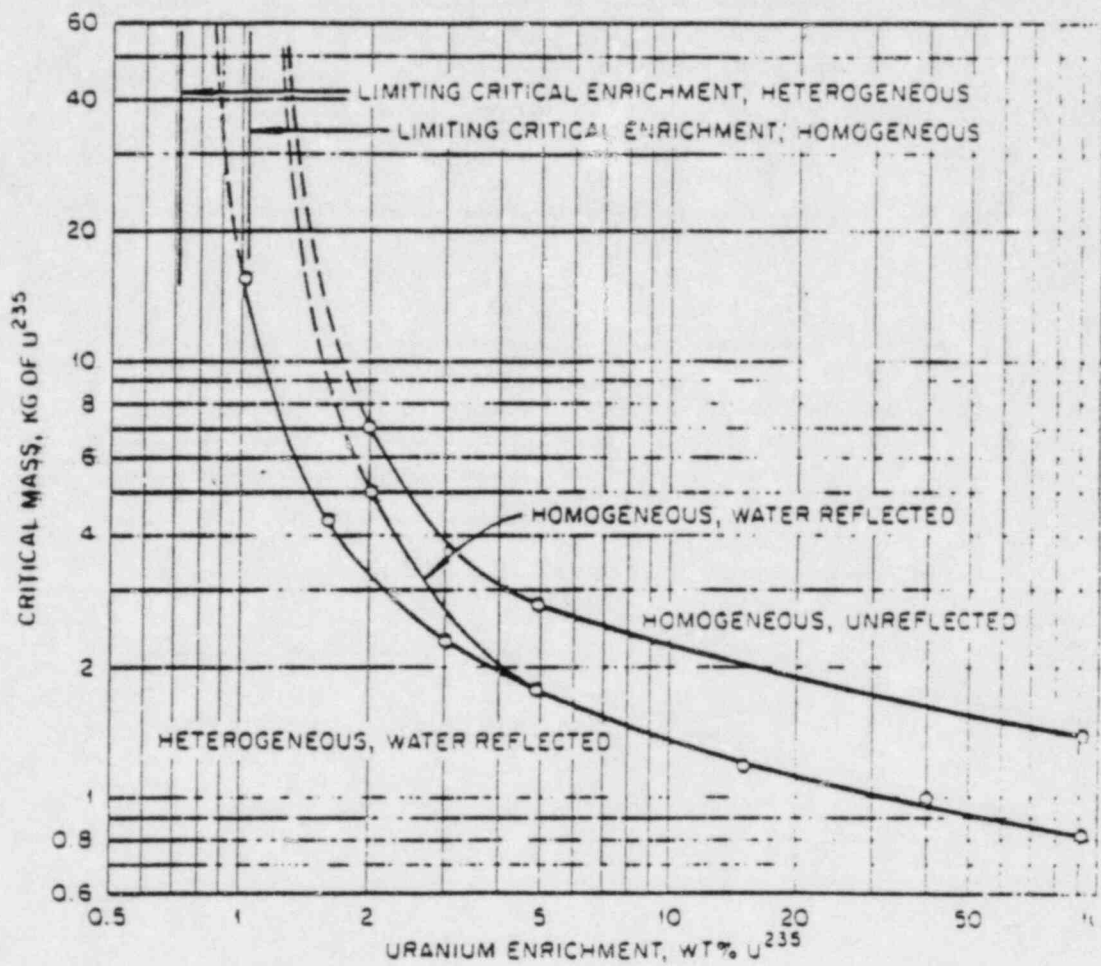


Fig. 2. Minimum critical mass as a function of U²³⁵ enrichment in hydrogen-moderated systems

REFERENCES

1. Paxton, H.C., Thomas, J.T., Callihan, D., and Johnson, E.B., Critical Dimensions of Systems Containing U²³⁵, Pu²³⁹, and U²³³, TID-7028, Los Alamos Scientific Laboratory and Oak Ridge National Laboratory, Oak Ridge, TN, June 1964.
2. O'Dell, R.D. (editor), Nuclear Criticality Safety, compendium of information presented at the Biannual Nuclear Criticality Safety Short Course in Taos, NM by the University of New Mexico, May 1973, published by Technical Information Center, Office of Information Services, U.S. Atomic Energy Commission, Washington, D.C., 1973.
3. Radiation Sources Division Instruction, RSD 5-8, AFRRI/SSRS.

3. State the source(s) you relied on for your statement in the HSR that it takes approximately 67 closely packed fuel elements to achieve criticality.

Answer to Question 3.

Answered by: Sholtis, Moore, Smoker

A. This reference of experience is contained within AFRRI's internal Radiation Sources Division Instruction, RSD 5-8, "Reactor Core Loading and Unloading Procedures" and states that, "AFRRI-TRIGA Core II (stainless steel clad elements) attained criticality with 69 fuel elements, 2630 grams Uranium-235." This statement is based on actual core loading experience at AFRRI using the standard I/M approach to critical procedure. The actual number of AFRRI-TRIGA fuel elements required to achieve criticality in the core may vary slightly (i.e. ~1 to 2 fuel elements) depending on the loading order actually used. ✓

B. RSD 5-8, "Reactor Core Loading and Unloading Procedures," AFRRI/SSRS, 27 March 1981. A copy of this document is on file with the USNRC, Region I Field Office.

C. See general statement.

D. See general statement.

E. See general statement.

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REACTOR CORE LOADING AND
UNLOADING PROCEDURES

1. Purpose. To set forth the procedures to be followed by the Reactor Branch staff in the complete loading and unloading of the AFRRI-TRIGA reactor core.
2. Applicability. The provisions of this instruction are applicable to the Reactor Branch staff, and the Health Physics Division staff.
3. Cancellation. RSD Instruction 5-8 dated 14 December 1976 is hereby cancelled.

4. General.

a. The reactor core loading and unloading procedures contained herein apply to the preparation phase as well as the actual loading and unloading phases. These procedures are based on two (2) separate core loadings and one core unloading of the AFRRI-TRIGA reactor. Emphasis is placed on following the procedures specified herein to insure continuity of operation and retention of experience within the Reactor Branch.

b. All activities associated with either the loading or unloading of the reactor core will be recorded in the Reactor Operations Logbook.

c. The minimum number of personnel that will be required is (1) Physicist-in-Charge (PIC) or his designee, (2) Chief Supervisory Operator (CSO), (3) One NRC licensed reactor operator, and (4) Health Physics Division representative.

d. A daily Startup Checklist will be completed prior to the movement of any fuel elements.

e. An approved Special Work Permit will be initiated prior to the movement of any fuel elements, if and as required.

f. If any new fuel elements are to be used, each element must be inspected when received at AFRRI. Each element will be removed from its shipping container, cleaned, and inspected for visual defects. Length and bow measurements must also be made and recorded. Smears of the element cladding for alpha contamination must be performed by the Health Physics Division representative prior to being handled by Reactor Branch personnel.

g. If any new thermocouple elements are to be used, a thermocouple calibration will be performed. The fuel element will be placed in a water bath, and Emf readings will be recorded over the range 20-100 degrees Centrigade.

h. At no time will more than six (6) new fuel elements be out of their shipping containers and on the reactor room floor level.

i. The Physicist-In-Charge or his designee must directly supervise all sequences of loading and unloading the reactor core.

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j. An NRC licensed reactor operator will continuously observe the nuclear instrumentation at the control console during all movements of control rods and fuel elements.

k. No fuel element which has experienced burnup in the core shall be removed from the reactor pool unless at least two (2) weeks have transpired since its use in the core.

5. Nuclear Instrumentation.

a. The following nuclear instrumentation is the minimum required for a reactor core loading:

(1) Two ionization chambers will be located outside the core shroud, along the core centerline, and adjacent to core positions F-4 and F-12, respectively. The readouts for these chambers will be picoammeters, or equivalent.

(2) One BF₃ or fission chamber will be located outside the core shroud, along the core centerline, and adjacent to core position F-8. The readout for this chamber will be a scaler unit.

b. The minimum nuclear instrumentation required for the unloading of the reactor core is:

(1) One BF₃ or fission chamber will be located outside the core shroud, along the core centerline, and adjacent to core position F-8. The readout for this chamber will be a scaler unit.

c. An operational check of the channels will be made as follows prior to the movement of any fuel element.

(1) A neutron source (3-5 curies) will be placed in the neutron source holder, and an increase in the readings will be observed on all channels.

(2) The neutron source will be removed from the neutron source holder and the readings will be taken and recorded.

(3) Replace the neutron source in the neutron source holder, and then generate a bias curve for the startup channel identified in 5.a.(2) or 5.b. above as appropriate. Record all channel readings with the source. These measurements will be performed several times in order to obtain reasonable reproducibility. These readings will be the basis for future calculations of source multiplication only in the loading of a reactor core. The neutron source reading will be the difference between the readings with and without the neutron source in place in the reactor core.

d. The nuclear instrumentation will be turned on and allowed to stabilize prior to the movement of any fuel elements, or making measurements of source effect.

6. Core Loading.

a. A 1/M curve is obtained by plotting the inverse multiplication vs the amount of fuel added (total amount in the core). The inverse multiplication is the ratio of the source reading to the reading with the fuel added. The loading curve will seldom be a straight line but may be either concave or convex dependent upon the geometry (source-detector distance). Hence, a number of different channels will yield different predictions of criticality. Since not all channels will agree, a conservative approach will be taken and the smallest number of estimated fuel elements required for criticality will be used to dictate future steps.

b. The fuel elements will be loaded in accordance with Table 1.

TABLE 1
FUEL LOADING SCHEDULE

<u>STEP #</u>	<u># ELEMENTS</u>		<u>REMARKS</u>
	<u>ADDED</u>	<u>TOTAL</u>	
1	4	4	Load four thermocouple elements, 2 in the B ring and 2 in the C ring.
2	14	18	Complete loading of B and C rings.
3	15	33	Load D ring.
4	15	48	Load E ring positions 1, 2, 4, 6, 8, 9, 10, 12, 14, 16, 17, 18, 20, 22 and 24. This loading is designed to complete a compact array around the control rods as well as to fill water gaps.
5	9	57	Complete loading of E ring.
6	9	66	Load F ring in positions 1, 5, 9, 13, 17, 21, 22, 23, and 27.

c. After each step of the fuel loading, perform the following:

- (1) Record readings.
- (2) Withdraw control rods 50%.
- (3) Record readings.

- (4) Withdraw control rods 100%.
- (5) Record readings.
- (6) Calculate M, 1/M for the step.
- (7) Plot 1/M vs # fuel elements.
- (8) Plot 1/M vs weight of uranium-235.
- (9) Plot 1/M vs control rod position (50% and 100%).
- (10) Predict critical loadings.
- (11) Estimate worth of the control rods.
- (12) INSERT CONTROL RODS TO FULL "IN" POSITION.

d. AFRRI-TRIGA Core I (aluminum clad elements) attained criticality with 72 fuel elements, 2811.33 grams uranium-235. AFRRI-TRIGA Core II (stainless steel clad elements) attained criticality with 69 fuel elements, 2630 grams uranium-235.

e. Continue the loading sequence as detailed below until criticality is obtained, and until the excess reactivity is 40-50 cents:

TABLE I (Continued)

FUEL LOADING SCHEDULE

<u>STEP #</u>	<u># ELEMENTS</u>		<u>REMARKS</u>
	<u>ADDED</u>	<u>TOTAL</u>	
7	2	68	Load F ring positions 19 and 25.
8	2	70	Load F ring positions 3 and 11.

f. Prior to loading the core to an operational configuration, the following measurements will be made:

- (1) Control rod calibrations using the rod drop techniques.
- (2) The worth of fuel elements in the remaining vacancies (E and F ring) vs water, taken one at a time.
- (3) Estimate the core configuration for an excess reactivity of approximately \$3.20.

g. The loading sequence will continue in order to attain a critical configuration with the transient rod in the DOWN position. This is the basis for the excess reactivity estimate of approximately \$3.20.

TABLE I (Continued)

FUEL LOADING SCHEDULE

<u>STEP #</u>	<u># ELEMENTS</u>		<u>REMARKS</u>
	<u>ADDED</u>	<u>TOTAL</u>	
9	2	72	Load F ring positions 7 and 15.
10	4	76	Load F ring positions 2, 14, 18, and 29. Record critical rod Bank position; Calibrate the lower portion of the transient rod (0-25%) via the positive-period technique.
11	4	80	Load F ring positions 8, 10, 24, and 30. Calibrate the middle portion of the transient rod (25-75%) via the positive- period method.
12	2	82	Load F ring positions 16 and 20, and this should complete the operational configuration as stated above.

h. Calibrate the four control rods via the positive-period method, and then compute the excess reactivity in the reactor core (K-excess must not exceed \$5.00).

i. Complete the core loading, insuring that the K-excess does not exceed \$5.00.

TABLE I (Continued)

FUEL LOADING SCHEDULE

<u>STEP #</u>	<u># ELEMENTS</u>		<u>REMARKS</u>
	<u>ADDED</u>	<u>TOTAL</u>	
13	5	87	Load F ring positions 4, 6, 12, 26, and 28.

j. Recalibrate the four control rods via the positive-period method, and then compute the K-excess reactivity in the reactor core.

7. Core Unloading

a. The reactor core will be unloaded starting with the F ring and ending with the B ring.

b. The fuel elements will be individually removed from the reactor core, identified by serial number, and placed either in the fuel storage racks or a shipping cask.

c. If the fuel elements are to be loaded into a shipping cask, the following actions will be taken in preparing the shipping casks for loading:

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(1) A radiological survey will be made of the shipping cask upon arrival and before it will be removed from the truck.

(2) The cask will be moved from the truck to the Prep Area.

(3) The hatches, which provide access from the Prep Area to the Reactor Room, will be opened and the lifting hook to the power hoist lowered to the Prep Area.

(4) The power hoist will be operated in accordance with RSD Instruction 5-5.

(5) The lifting yoke will be attached to the cask and the cask lifted to the Reactor Room.

(6) The lid to the cask will be removed. The cask will be monitored by the Health Physics Division representative while the lid is being removed, to insure that no radioactive material is inside the cask.

(7) The inside of the cask will be smeared for gross alpha and beta contamination.

(8) The inside of the cask will be vacuumed. The inside and outside of the cask will be washed down. The water drain line on the cask will be checked to insure that it is not blocked. Also verify the operability of the pressure relief valve and the temperature sensing thermocouple.

(9) If more than seven elements are to be loaded into the cask, it will be necessary to verify that a thermal neutron poison is present in the cask to prevent the loading of a critical mass.

(10) Move the cask by crane from the reactor deck and position the cask in the reactor pool.

d. Load the cask with up to as many fuel elements as allowed by the license for the cask. If grid index markings are present in the cask, record which fuel element is placed in which grid position.

e. Lower the lid to the cask into the pool, place the lid on the cask, and secure the lid.

f. Raise the cask from the pool, drain the water from the cask into the pool, and then dry the cask off. The cask will be monitored while being removed from the pool to insure that no radiation hazard exists as a result of a weakness in the shielding in the cask. The cask will be smeared for gross alpha and beta contamination.

g. An air sample will be taken from the cask to measure the activity of the air. The data from all radiological surveys will be recorded.

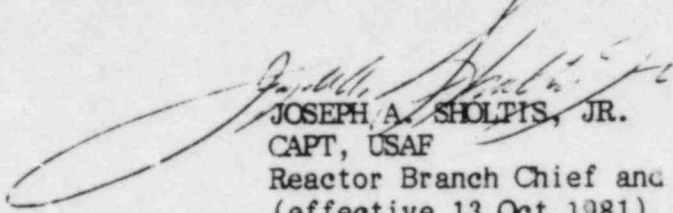
h. After the air sample has been taken, observe the temperature and pressure inside the cask until the temperature and pressure reach an equilibrium.

RADIATION SOURCES DIVISION
INSTRUCTION NUMBER 5-8

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i. Label the cask accordingly and complete the appropriate paperwork either for temporary storage or for transporting.

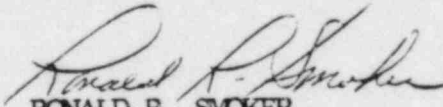
j. Move the cask to either a temporary storage area or to the truck for transporting. If the cask is to be placed in temporary storage, a criticality monitor must be available in accordance with 10 CFR 70.



JOSEPH A. SHOLTIS, JR.

CAPT, USAF

Reactor Branch Chief and Physicist-In-Charge
(effective 13 Oct 1981)



RONALD R. SMOKER

MAJ, EN, USA

Chief, Radiation Sources Division

ATTACHMENT 4

AFFIDAVIT
OF
JOSEPH A. SHOLTIS, JR.

Joseph A. Sholtis, Jr., being duly sworn according to law, deposes and says:

First, it should be pointed out that experiment failures with an assumed concurrent malfunction of confinement safeguards are explicitly addressed in the AFRRRI SAR on pages 6-8 and 6-9, section 6.2.5. (A copy of this SAR analysis section is provided as an attachment to this affidavit.) Therefore, such an accident is not of a "different kind" than those that are treated in the AFRRRI SAR. This analysis considers the worst-case experiment failure at AFRRRI which involves the irradiation of 20 liters of Argon gas at a steady state power level of 1.0 Mw(t) for 1 hour, and results in the production of 5.6 Curies of Ar-41 gas -- all of which is presumed to be released to the unrestricted area as a result of the experiment failure concurrent with a presumed total failure of confinement isolation system safeguards. At this point no probability estimates for such an event were even considered nor were any mechanistic ways of achieving such a series of events considered. That is, the worst experiment failure with total release to the environment was simply assumed to occur without regard to how or with what probability. The associated consequences to the public for this hypothetical worst-case experiment failure with complete release to the unrestricted environment involves a total whole-body dose of 2.7 mrad to an individual at or beyond 25 meters from the AFRRRI Facility, i.e. at or beyond the AFRRRI site boundary. This is hardly an accident having "greater severity" than the designated DBAs for the AFRRRI reactor facility which are treated in the AFRRRI SAR.

Licensee agrees wholeheartedly with the Intervenors that experiment failures can indeed occur and that malfunctions also can occur, but not necessarily concurrently with any

credible probability. However, we would be remiss by not trying to reduce their likelihood of occurrence, particularly for concurrent failures. We would similarly be remiss by not trying to limit the consequences of such a series of events, should they nevertheless occur and result in a release to the environment. AFRRI has, in place, an extensive surveillance, testing, and preventive maintenance program to detect equipment malfunctions and failures; in most cases, prior to reactor power operations taking place. As an example, numerous component, subsystem, and system preoperational functional surveillance checks, specifically designed to verify proper system functionality to ensure safe reliable operation, are required and must be performed and checked daily before any reactor power operations are permitted. AFRRI also has, in place, additional surveillance checks and preventive maintenance tasks that must be performed weekly, monthly, quarterly, semi-annually, and annually. In general, these are all identified and required within the AFRRI Reactor Technical Specifications. Such a system of surveillance, testing, and preventive maintenance does not preclude even single equipment failures or malfunctions but it does reduce their likelihood and provides reasonable assurance that such failures will be detected in a timely fashion.

Almost all of the malfunctions cited by the Intervenor under this contention were detected by an operator during the normal reactor preoperational start-up checkout procedure and, thus, before any power operation actually took place. In addition, almost all of the malfunctions cited by the Intervenors in this contention have nothing whatsoever to do with the generation of a source term, the operability of confinement isolation safeguards, or a release to the environment. These specifics will be addressed in more detail below where each malfunction cited by the Intervenor under this contention is discussed individually.

Also from an accident probability minimization viewpoint, redundant and independent "back-up" systems are often provided such that all must fail before functionality is actually lost.

This is the case for several of the system malfunctions which Intervenor cites in this contention. This point will also be appropriately discussed in more detail when each of the Intervenor's cited malfunctions are addressed individually below.

From the standpoint of limiting the consequences of a release to the environment, should one occur, for example, as a result of an experiment failure with an assumed concurrent total failure of the confinement isolation safeguards, AFRRRI has, in place, a system involving a body of experts for reviewing, approving, and limiting irradiations of various materials on a case-by-case basis for each material or experiment. This reviewing and approving body is the AFRRRI Reactor and Radiation Facility Safety Committee (RRFSC), which is required under the AFRRRI Technical Specifications, is composed of technical experts from within and outside of AFRRRI. Prior to any experiment utilizing the AFRRRI Reactor, the experiment and experimental protocol must be presented before the RRFSC for review and approval. The AFRRRI RRFSC subsequently must issue approved "special" or "routine" authorizations for the intended reactor use. These authorizations establish limits on the quantities of materials which can be irradiated, based on the safety and radiological implications of each, should an experiment failure occur together with a failure of confinement isolation such that the entire inventory of activated materials is released to the environment. In addition, these authorizations also establish limits on the use of the various experimental apparatuses and exposure facilities that exist or are proposed for use.

Intervenor has admitted that multiple failures or malfunctions are generally required to achieve conditions for a release to the unrestricted environment. (See transcript of NRC Staff's deposition of Dr. Irving Stillman on 18 Dec 1982 in New York on page 83 line 4 through page 84 line 15, inclusive, and also on page 85 lines 7 through 24, inclusive.) Further, the Intervenor has admitted that such multiple failures are successive and independent and

must occur concurrently in time. (See page 83 lines 11 through 13, inclusive, of the transcript of Dr. Stillman's Deposition on 18 Dec 82 in New York.) Moreover, the Intervenor has indicated that it has not gone through the thought process to postulate a scenario of events involving malfunctions that would ultimately lead to a release to the unrestricted environment. (See page 79 line 7 through page 85 line 24, inclusive, of the transcript of Dr. Stillman's Deposition on 18 Dec 82 in New York.) Intervenor has repeatedly failed to supply information to the Licensee upon request showing the relationship between each of the Intervenor's cited malfunctions under this contention and the generation of a source term, their relationship to confinement safeguards, and their relationship to a release to the environment -- given that each cited malfunction is presumed to occur. (See, for example, Licensee's Motion to Compel served 15 Jan 1982 and as supplemented 24 Feb 1982.) In fact, to this day, Intervenor has still not provided this information even though Dr. Stillman promised to provide it when asked during his deposition in New York on 18 Dec 1982. (See page 78 line 7 through page 85 line 24, inclusive, of the transcript of the deposition of Dr. Irving Stillman on 18 Dec 82 in New York.)

Licensee submits that single independent failures can occur but their probability is reduced by virtue of performing routine scheduled preventive maintenance, and their impact is considerably reduced by virtue of having redundant and independent "back-up" systems and by performing routine recurrent surveillance checks and tests. The probability of single independent failures is admittedly low but nevertheless they are expected, but the probability of multiple independent failures is certainly well below that for single failures which makes them extremely unlikely events, particularly if they are to occur concurrently in time. Still, if they should occur and an environmental release results, limitations set-down by the RRFSC would limit the available release source term and, therefore, also limit the

consequences of such extremely remote sequences of failures leading to an environmental release. More importantly, releases due to experiment failure together with confinement isolation failure have been addressed in the AFRRRI SAR.

Next, we will focus on each of the malfunctions which Intervenor cites as supporting evidence in this contention. The first of these is a breach of containment caused by missing rubber gasket sealing material on the double doors to the corridor behind the reactor control room in 1978. This oversight did constitute a violation of Licensee's Technical Specifications and a notice of violation was issued. However, Licensee was in the process of installing the gasket material when the condition was noted by NRC, and the reactor room was still capable of establishing and maintaining a negative pressure even without the gasket material in place so long as the doors were closed and the reactor room was isolated by closing of the ventilation dampers. Therefore, although this condition did represent a literal violation of the Technical Specifications, it did not in actuality significantly negate the capability of the confinement isolation safeguards. Also, if one assumes that confinement isolation was in fact compromised at this internal doorway point, a second set of doors to corridor 3106, also behind the reactor control room, would have confined any airborne source term to the reactor facility confines since this second set of doors did and does still have gasket sealing material installed in place. Nevertheless, any Technical Specification violation, particularly a condition involving missing rubber gaskets on confinement area doors or any other potential compromise to the confinement isolation boundary, is viewed by the Licensee as a valid concern. In this regard, steps have been taken to remedy such situations and ensure they will not occur again in the future. (See NRC Inspection reports on the AFRRRI reactor facility under Docket 50-170 since 1978.)

The second malfunction Intervenor cites under this contention is a failure, on 26 August

1975, of the reactor room ventilation dampers to close when the Continuous Air Monitor was alarmed. This malfunction has no relationship with the generation of a source term. The reactor room ventilation dampers are designed to close (by a fail-safe air-actuated solenoid) automatically upon receipt of a high-level alarm of the reactor room Continuous Air Monitor (CAM) or upon receipt of a manually-initiated signal by an operator in the reactor control room. Part of the normal daily preoperational reactor startup checkout procedure involves manually initiating a high-level CAM alarm (artificially) to check the operability of the CAM as well as the closure of the reactor room ventilation dampers. On this particular day, 26 August 1975, the operator performed this preoperational checkout item, but the dampers failed to close. As a result of this failure, reactor power operations did not occur and were not permitted to take place until repair was effected. This is a prime example of exactly why we have such preoperational checks. As a result, this failure could not have contributed to a release to the environment, since it was detected and no operations to produce a potential source term were performed. Even if it had not been detected and power operations did occur and a source term somehow were generated, numerous other radiation monitoring devices are available, with audio and visual alarms, to detect the event and alert the operator so that manual damper closure could be effected. This is true since the operability of the dampers was unaffected by this malfunction; the only effect of this malfunction was the loss of an automatic signal to the ventilation dampers for closure.

The third malfunction which Intervenor cites is a failure of the in-pool lead shielding doors to stop opening at the fully opened position in August 1976. This malfunction has no relationship to confinement isolation safeguards, the generation of a source term, or a release to the environment. The purpose of the in-pool lead shielding doors, when closed, is to provide adequate gamma photon attenuation or shielding such that one exposure room can be safely occupied for experiment set-up while the reactor is operating at power at the

other exposure room at the opposite end of the tank. Since there is a potential for physical contact between the core and the lead shielding doors in certain regions of the pool, microswitches, interlocks, slip clutches, a core shroud, a TV monitor, and administrative controls have been installed/established to either preclude such contact or, in the extremely unlikely event that contact nevertheless does occur, to minimize fuel element damage. The malfunction cited by the Intervenor in this case precludes operation of the reactor at power due to an additional and separate interlock that prevents the supplying of current to the standard control rod drives and prevents supplying air to the transient control rod drive unless the lead shielding doors are either fully open or fully closed. In addition, this malfunction precludes moving the core dolly into the mid-pool region where physical contact might occur since an additional interlock system only permits core dolly travel into the mid-pool region when the lead shielding doors are fully open. As a result, there was no potential for a source term or release to the environment by virtue of this malfunction. It should also be pointed out that this malfunction was immediately detected by the operator and repairs were effected before operations were permitted to resume.

The fourth malfunction cited by the Intervenors in this contention involved a reactor core position safety interlock malfunction on Feb 1, 1973. This malfunction, like the third discussed above, has no relationship with confinement isolation safeguards, the generation of a source term, or a release to the unrestricted environment. This malfunction also was detected immediately by the operator and repairs were effected before operations were permitted to resume. This malfunction could have resulted in the core shroud physically contacting the lead shield doors near the center of the pool if operator error had additionally been involved. However, the core dolly drive slip clutch, which was operational, and the core shroud would have minimized any impact damage and not permitted the fuel elements to have been contacted at all. Even if a clad failure were assumed to result from

such a malfunction and operator error, a source term would have been generated but no pathway to the environment would have been provided since the confinement isolation system was unaffected, and even if it too were presumed to fail, a clad failure event has been addressed in the AFRRI SAR and its consequences are minimal. While one can never rule out such a release, its likelihood is made extremely remote by virtue of the protective systems and design features provided. Nevertheless, the ultimate consequences of a release are analyzed within the AFRRI SAR.

The fifth malfunction that the Intervenor cites in this contention involved a malfunction of High Flux Safety Channel #1 to initiate a scram signal on March 15, 1980. Regardless of what the Intervenor actually says in this contention, this malfunction was detected by an AFRRI reactor operator, not an NRC inspector, and the detection occurred during a normal daily preoperational startup checkout. (See Intervenor's initial and supplemental response to Licensee's first-round interrogatory #16e.) As a result of preoperational detection of this malfunction by an operator, power operation did not take place and, therefore, no potential for an experiment failure or the generation of any other source term existed. Just as in the third and fourth malfunctions discussed above, this malfunction also has no relationship to confinement isolation safeguards, the generation of a source term, or a release to the environment. The purpose of high flux safety channels one and two, which are redundant and independent, is to provide redundant readouts of reactor power level and to initiate a reactor scram if a 1.1 MW(t) steady state power level is attained. Each day that reactor power operations are planned, the reactor operator is required to first perform a preoperational startup checkout procedure which involves, in part, placing test signals on safety channels one and two separately to simulate a power increase and to check that a scram signal is generated by each channel at or below a 1.1 MW(t) indicated power level. It was during just such a preoperational check that this malfunction was detected. Even if it

had not been detected and power operations had been conducted and a power level of 1.1MW(t) was attained by virtue of operator error, high flux safety channel #2 would still have generated a scram at 1.1 MW(t) or, if it too somehow were presumed to fail, two independent and redundant fuel temperature channels would still have been available to scram the reactor upon attainment of 500°C fuel temperatures and, even if they too were somehow assumed to be malfunctioning, the maximum steady-state power attainable with the AFRRI reactor is only approximately 1.4 MW(t) which has an associated fuel temperature of less than 600°C and, therefore, no damage, source term, or release would be expected. And even if a release to the reactor room did somehow occur, confinement isolation was still available, and even if it too were assumed to fail, the consequences have been determined in the AFRRI SAR and they are minimal. This represents an incredible series of events.

The sixth malfunction that Intervenor cites in this contention involved a reactor exhaust system malfunction on August 9, 1979 caused by an electrical fire in the EF-1 cubicle of the motor control center, in turn caused by a power surge due to a faulty transformer. This malfunction, like numbers 3, 4, and 5 above has nothing whatsoever to do with confinement isolation integrity, the generation of a source term, or a release to the environment. This event simply involved a minor electrical fire (not associated with the reactor or its safety systems) and a resultant loss in ability to draw air from the reactor room through high-efficiency particulate air filters and out the AFRRI stack. It had no impact on the ability of the confinement isolation dampers to close nor can it contribute to an experiment failure or the generation of any other source term. This particular malfunction occurred while the reactor was not operating, but if it had been operating, stack flow audio-visual alarms would have alerted the operator that no stack flow was being provided and the operator would have been forced to manually scram the reactor under existing procedures. Even if the operator failed to scram the reactor in the face of both audio and visual alarms, no potential for a

source term exists, and even if one was presumed to somehow occur, the confinement isolation system was still operational, and if it too somehow managed to fail, the consequences of the release have been determined and they are minimal -- another incredible series of events.

The seventh malfunction Intervenor's cite in this contention involved a malfunction of the fuel element temperature sensing circuit #2 caused by a floating signal ground on August 1, 1979. In order to simplify the discussion from here on, only major pertinent points will be identified henceforth. This malfunction:

- 1) was detected by an operator during a normal preoperational checkout so that power operation did not take place.
- 2) is protected by a redundant and independent fuel temperature sensing channel #1 and other available safety channels.
- 3) does not have anything to do with confinement isolation, generation of a source term, or a release to the environment.

The eighth malfunction that the Intervenor's cite under this contention, involved a malfunction, in July 1979, of the pool water level sensing float switch caused by wear on the jacketing around the wires leading to the switch. This malfunction:

- 1) was detected by an operator during a normal surveillance check; power operations were curtailed until repairs were effected
- 2) is protected by numerous radiation monitoring devices that would also indicate, via shine, any substantial loss of pool water should that occur
- 3) has nothing whatsoever to do with confinement isolation, generation of a source term, or a release to the environment.

The ninth malfunction that Intervenor cites in this contention involved a malfunction of the Reactor Room CAM to initiate a signal for closing the reactor room ventilation dampers on 26 August 1975. This malfunction is the same malfunction that Intervenor cites earlier and which is discussed above as the second malfunction. This malfunction:

- 1) was detected by an operator during a normal preoperational checkout so that reactor power operations did not take place
- 2) is protected by numerous other available radiation monitoring and detection devices, with audio-visual alarms, and by the capability for an operator to manually initiate damper closure
- 3) has nothing whatsoever to do with generating a potential source term or negating the physical operability and integrity of confinement safeguards.

The tenth malfunction that the Intervenor cites under this contention involved a malfunction of the fuel temperature automatic scram system on January 29, 1974 caused by a build-up of high resistance material on the mechanical contacts of the T2 output meter. This malfunction:

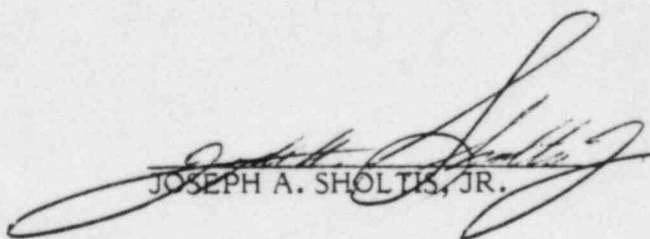
- 1) was detected by an operator during a normal preoperational checkout; no operations were permitted until repair was effected
- 2) is protected by the redundant and independent fuel temperature sensing channel #1 and other available safety channels
- 3) can no longer occur with the existing new console design
- 4) has nothing whatsoever to do with confinement isolation, the generation of a source term, or a release to the environment.

The eleventh and last malfunction that the Intervenor cites under this contention involved a malfunction of the reactor core position safety interlock system on February 1, 1973 caused

by a faulty de-energizing relay. This malfunction is the same malfunction that Intervenor cites earlier and which is discussed above as the fourth malfunction. This malfunction:

- 1) was detected immediately by the operator; no operations were permitted until repair was effected
- 2) is protected by the core dolly drive slip clutch, the core shroud, TV monitoring, operator action, interlocks, and administrative controls
- 3) has nothing whatsoever to do with confinement isolation, the generation of a source term, or a release to the environment.

This discussion points out the designed protective features and redundancies provided for the reactor facility. It also demonstrates the extreme improbability, and often impossibility, of actually getting a release to the unrestricted environment. Moreover, it illustrates the limitations Licensee imposes on its own irradiations of materials to restrict the consequences of a release should one occur.


JOSEPH A. SHOLTIS, JR.

Sworn to and subscribed before
me on this 25th day of Feb, 1983.

My Commission expires:
April 8, 1984
Miriam E. Lesley
Notary Public

6.2.5 Experiments

All experiments performed as part of the TRIGA reactor operations are reviewed by the Reactor and Radiation Facility Safety Committee and must be authorized prior to their performance. The technical specifications contain requirements that must be met before such experiments can be performed using the AFRRI-TRIGA reactor. Experiments are always supervised by trained, licensed, supervisory personnel. However, failure of an experiment is possible and worst-case conditions can be calculated to determine the postulated consequences.

The two worst-case conditions for failure of an experiment could result in instantaneous insertion of reactivity or the release of radioactive material from an experiment undergoing activation in the reactor. For an experiment failure in which reactivity could be added, the worst possible case would be the prompt addition of less than 0.36% $\Delta k/k$ in either Exposure Room 1 or 2. As discussed for the case of improper fuel loading (Section 6.2.3), the addition of 0.36% $\Delta k/k$ would be within the range of an improper fuel loading condition. Such an addition would not result in any damage to the reactor or the fuel.

For an experiment failure in which radioactive material could be released from the experiment, i.e., activation products, the worst case would be the prompt release of the radioactive material to the atmosphere. An authorized experiment involves the irradiation of 20 liters of argon gas for 1 hour at a power level of 1 MW. The resulting activation would result in a total Ar-41 activity of 5.6 Ci in the sealed container. If the container should fail and release all of the Ar-41 activity, the resulting total whole body dose would be less than 2.7 mrad to an

individual more than 25 meters from the AFRRI facility (Equation 3, Section 6.3.4.1). The failure of this authorized experiment represents the worst case for radiological consequences from an experiment failure in the AFRRI-TRIGA reactor. Such a whole body exposure would not represent an undue risk to the health and safety of the general public.

ATTACHMENT 5

AFFIDAVIT
OF
JOSEPH A. SHOLTIS, JR.

Joseph A. Sholtis, Jr., being duly sworn according to law, deposes and says:

A negative temperature coefficient of reactivity simply means that as the temperature increases, negative reactivity is inserted which acts to reduce/shutdown the fission process. Every reactor designed and operated in this country must have a negative temperature coefficient of reactivity; TRIGA reactors are no exception. For stainless-steel clad TRIGA fuel elements containing a fuel-moderator mixture of U-ZrH_{1.7}, which is the fuel used in the AFRRRI reactor, the prompt negative temperature coefficient of reactivity has a value of $-1.26 \times 10^{-4} \Delta k/k/^\circ\text{C}$ or $-1.8\text{¢}/^\circ\text{C}$. This value is contributed to, in a cumulative fashion, by three major separate and independent factors. They are, in order of their importance: 1.) The ZrH_x or cell factor, 2) the Doppler broadening factor, and 3.) the density decrease or leakage factor - each of which is in and of itself negative with increasing temperature. (See the attached General Atomics Information sheet.) If we define or designate the overall TRIGA reactor's temperature coefficient of reactivity as α_T , then we can write an equation for α_T which shows its constituent parts or terms as:

$$\alpha_T = \alpha_{\text{ZrH}_x} + \alpha_{\text{Doppler}} + \alpha_{\text{Density}}$$

where: α_{ZrH_x} is the ZrH_x (or cell) effect term
 α_{Doppler} is the Doppler broadening effect term, and
 α_{density} is the density decrease (or leakage) effect term.

Both the Doppler broadening factor and the density decrease (or leakage) factor will individually always be negative with increasing temperatures, regardless of whether the fuel is damaged and hydrogen is presumed lost or not. The Doppler broadening factor is negative and inherent to any thermal reactor system fueled with uranium, which includes TRIGA reactors. Uranium, particularly the uranium-238 isotope, has numerous high value resonance capture peaks in its absorption cross-section at epithermal energies. These resonance capture peaks broaden with increasing temperature, such that fast fission neutrons undergoing moderation toward thermal energies have a lesser chance (as temperature increases) of actually reaching thermal energies (where fission predominantly occurs), without being captured parasitically (via radiative capture) by one of these broadened resonance capture peaks in the uranium. In essence, Doppler broadening acts to reduce the resonance escape probability, p , which is one of the six factors in the six factor formula for k -effective, and thus, introduces negative reactivity in a thermal reactor fueled with uranium as the fuel temperature increases. This factor, which is discussed in detail in numerous nuclear engineering texts, will always be negative, inherent, and unalterable in TRIGA fuel so long as uranium is present. Therefore, this contributing factor to the TRIGA reactor's negative temperature coefficient of reactivity, would be unaffected by the Intervenor's postulated loss of hydrogen from damaged TRIGA fuel and, thus, would always remain negative. This factor alone would ensure automatic reactor shutdown during pulse operations since it by itself is negative and the two remaining factors can be made no larger than zero at best - and even this last postulate would require a negation of the laws of nature.

The density decrease (or leakage) factor similarly will always be negative, inherent, and unalterable with increasing temperatures in TRIGA-fueled cores (regardless of whether the fuel is damaged and hydrogen is presumed lost or not), so long as nature continues to ensure

that material densities decrease as temperatures increase. This factor not only involves density decreases in the fuel and the interstitial water between the fuel elements during heatup but also includes expansion effects of the fuel and the fuel elements (and thus the overall core) with increasing temperatures which results in an overall volumetric core increase and the displacement of some of the interstitial water moderator from between the fuel elements, by virtue of individual fuel element expansion, with heatup. These effects, taken collectively, not only act to reduce the effective moderation of neutrons toward thermal energy (i.e. Σ_s for the moderator decreases with heatup) but also decrease the macroscopic fission cross-section, Σ_f , in the fuel (and thus decrease material buckling, B_m) and also increase the likelihood that neutrons will leak from the core without contributing to fission by virtue of the core volumetric increase with heatup which, in turn causes an increase in the geometric buckling, B_g . Each of these individual effects is negative so that, overall, the density decrease (or leakage) factor must always be negative with increasing temperature, regardless of whether hydrogen is presumed lost from damaged TRIGA fuel or not. Just as in the case of the Doppler broadening factor, the density decrease (or leakage) factor is also sufficient, by itself, to ensure automatic reactor shutdown during pulsing, regardless of whether the fuel is damaged and hydrogen is presumed lost or not, since it (α_{Density}) is always negative, inherent, and unalterable, and the other two factors cannot ever become positive. Moreover, as we have demonstrated above, both the Doppler broadening and the Density decrease factors will always each be negative, regardless of whether the TRIGA fuel is damaged and hydrogen is presumed lost or not, so that collectively they are cumulative in a negative sense, making the TRIGA reactor's temperature coefficient of reactivity even stronger in a negative sense even if the ZrH_x (or cell) effect factor is presumed to somehow be forced to its maximum theoretical value of zero.

The zirconium-hydride, ZrH_x , (or cell) effect term theoretically could be forced to a maximum value of zero (but no larger) by somehow removing or driving out all of the hydrogen from all of the TRIGA fuel loaded in core, but even this action would in no way alter the negativeness of the Doppler broadening factor or the density decrease (or leakage) factor, and so the TRIGA reactor's overall temperature coefficient of reactivity would still remain negative regardless of whether hydrogen is presumed lost from damaged TRIGA fuel or not. However, the removal of hydrogen from a TRIGA fuel element will understandably reduce effective moderation of neutrons to the thermal energy region (where fission predominantly occurs) and this would, in and of itself, constitute a negative reactivity effect, albeit not associated with a temperature change. In fact, if all of the hydrogen was somehow assumed to be lost or driven from the TRIGA fuel elements in the core, it would be impossible to even attain criticality. This indicates that the hydrogen contained in the TRIGA fuel elements contributes to effective neutron moderation and, therefore, is an integrally important (actually required) moderator in a TRIGA reactor core. Therefore, a presumed total loss of hydrogen from all of the TRIGA fuel elements in core, although it would zero out the ZrH_x (or cell) effect term or contribution to the TRIGA reactor's overall negative temperature coefficient of reactivity, would in and of itself constitute a constant and large negative reactivity effect, and even this can not make the overall temperature coefficient of reactivity in a TRIGA reactor ever become zero or positive. Moreover, a presumed loss of hydrogen from TRIGA fuel would not permit fission to effectively occur in those elements in which hydrogen was presumed lost.

Up to this point, wholesale hydrogen loss from TRIGA fuel has simply been presumed and nothing has been said about the feasibility of driving hydrogen completely out of all the fuel elements in core (i.e., not just damaged TRIGA fuel elements), even though this is exactly what is necessary to force the ZrH_x (or cell) effect term to zero in the TRIGA reactor's

overall temperature coefficient of reactivity equation. Let's look at the feasibility of driving hydrogen out of TRIGA fuel on a per-element basis. If, from a worst-case point of view, it was assumed that the stainless-steel clad on a TRIGA fuel element loaded in core were magically removed in total and the reactor was operated such that fuel temperatures in this unclad element were maintained at 600°C (the fuel temperature scram point for the AFRRRI reactor), only a small fraction of the hydrogen within this unclad element would actually be lost. This statement is based on the hydrogen equilibrium pressure within TRIGA fuel as a function of temperature. (See Figure 2-9, attached, which is from GA Report # E-117-833, "The U-ZrH_x Alloy: Its Properties and Use in TRIGA Fuel," by M.T. Simnad, page 2-13, February 1980.) This attached Figure indicates that at 600°C, the equilibrium hydrogen pressure is only approximately 1 psi. (Note also from this Figure that you would have to go to a temperature of approximately 775°C before a 1 atm equilibrium hydrogen pressure is reached.) Since the core is submerged under approximately 14-18 feet of water, where the pressure exerted by the water on the surface of the fuel at this depth is greater than 1 atmosphere (approximately 1.4 atm), there would be no driving force (except for normal thermal diffusion) to remove significant amounts of hydrogen from a failed TRIGA element. Within a TRIGA element hydrogen tends to migrate from hot to cooler regions and then recombine with free zirconium during power operation. Therefore, since radial and axial temperature gradients are established within individual TRIGA fuel elements during power operation and since the water pressure exerted on an element is greater than the equilibrium hydrogen pressure within an element up to approximately 775-800°C (See attached Figure 2-9), it is expected that hydrogen would simply redistribute within an element (to some extent) from hot to cooler regions, but loss of hydrogen from a failed TRIGA element would be very small since it would be due to thermal diffusion only. This certainly does not constitute a way of driving or removing hydrogen from a failed TRIGA fuel element in a wholesale fashion. It is, therefore, extremely unlikely that hydrogen could

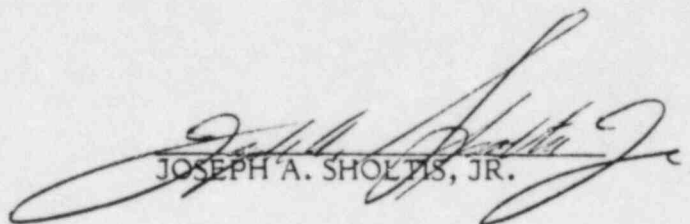
be removed from failed TRIGA fuel in a gross way. This in turn, indicates that it would be extremely unlikely (and difficult) to even force the ZrH_x (or cell) effect term, in the TRIGA reactor's overall temperature coefficient of reactivity equation, to zero -- which, even if it were presumed to occur, would still not force the TRIGA reactor's overall temperature coefficient of reactivity to a zero or positive value, i.e., overall, α_T would still remain negative, even if the ZrH_x term were presumed to go to its theoretical maximum value of zero.

Intervenor has admitted that if all the hydrogen contained in the TRIGA core's fuel elements were presumed lost, criticality could not be achieved. (See page 90 lines 2-5, inclusive, and page 91 lines 4 through 16, inclusive, of the transcript of Dr. Irving Stillman's deposition on 18 Dec 1982 in New York.) This statement alone by Intervenor indicates the importance of the hydrogen contained in the TRIGA fuel as a moderator and, in fact, the requirement of this hydrogen contained within TRIGA fuel for the reactor to operate (via fission). Intervenor has also admitted that this gross effect (i.e. total loss of hydrogen from the TRIGA fuel in core and the associated inability, by virtue of its large negative reactivity effect, to achieve criticality via fission) is contributed to by each TRIGA fuel element assumed to be damaged in which hydrogen is presumed to be lost. (See page 91 line 17 through page 92 line 10, inclusive, of the transcript of Dr. Irving Stillman's deposition on 18 Dec 1982 in New York.) Moreover, Intervenor further has admitted that each damaged TRIGA fuel element, which is presumed to have lost all or part of its hydrogen, contributes to the overall large corewide negative reactivity effect that total hydrogen loss from the entire core's TRIGA fuel elements would introduce. (See page 92 line 11 through page 93 line 8, inclusive, of the transcript of Dr. Stillman's deposition taken in New York on 18 Dec 1982.) These admissions by the Intervenor indicate that any damaged TRIGA fuel element loaded in core which is presumed to have lost part (or all) of its hydrogen, will be less and

less effective (with increasing hydrogen loss) in contributing to the core's neutron population, power level, and fission density. This indicates that any damaged TRIGA fuel element loaded in core, which is presumed to have lost some (or all) of its hydrogen, will have a decreased neutron population, power contribution, fission density, and thus fuel temperature, which becomes further and further reduced as hydrogen loss increases, in comparison with normal undamaged TRIGA fuel elements in core. This, in turn, indicates that locally within the core wherever a damaged TRIGA fuel element (with presumed hydrogen loss) exists, a constant negative reactivity effect also exists, i.e., this local negative reactivity effect is not temperature dependent. Therefore, overall you would not only have a negative reactivity temperature feedback effect (via the ZrH_x or cell effect) as long as some (i.e. any) hydrogen exists within any of the core's TRIGA fuel, but you would also have a negative reactivity temperature feedback effect via the Doppler broadening and density decrease (or leakage) effects, and further you would also have a constant negative reactivity effect in each TRIGA element that is presumed to have lost a significant portion of its hydrogen. Licensee is, thus, at a loss to see how the Intervenor can possibly contend that the TRIGA reactor's negative temperature coefficient of reactivity could ever become zero or positive and, thus, ever fail to shutdown the reactor automatically during a pulse.

One last point should be made. If a TRIGA element were to fail (i.e. a clad failure occurs) and hydrogen were presumed to be driven out or lost from the failed element, fission gases would also be released to the pool water. These would be detected via the activation of radiation alarms. Under such a scenario, it is hard to believe that any subsequent reactor power operations would be permitted, i.e. with obviously damaged fuel. And finally, such accidents involving clad failure are treated in the AFRRRI SAR and, therefore, can not be

considered to be of a different kind or greater severity than those that are treated in the
AFRRI SAR.


JOSEPH A. SHOLTIS, JR.

Sworn to and subscribed before
me on this

25th day of Feb, 1983.

My commission expires:
April 8, 1984

Miriam E. Lesley
Notary Public

APPENDIX C

A BRIEF DISCUSSION OF THE TRIGA PROMPT NEGATIVE TEMPERATURE COEFFICIENT OF REACTIVITY

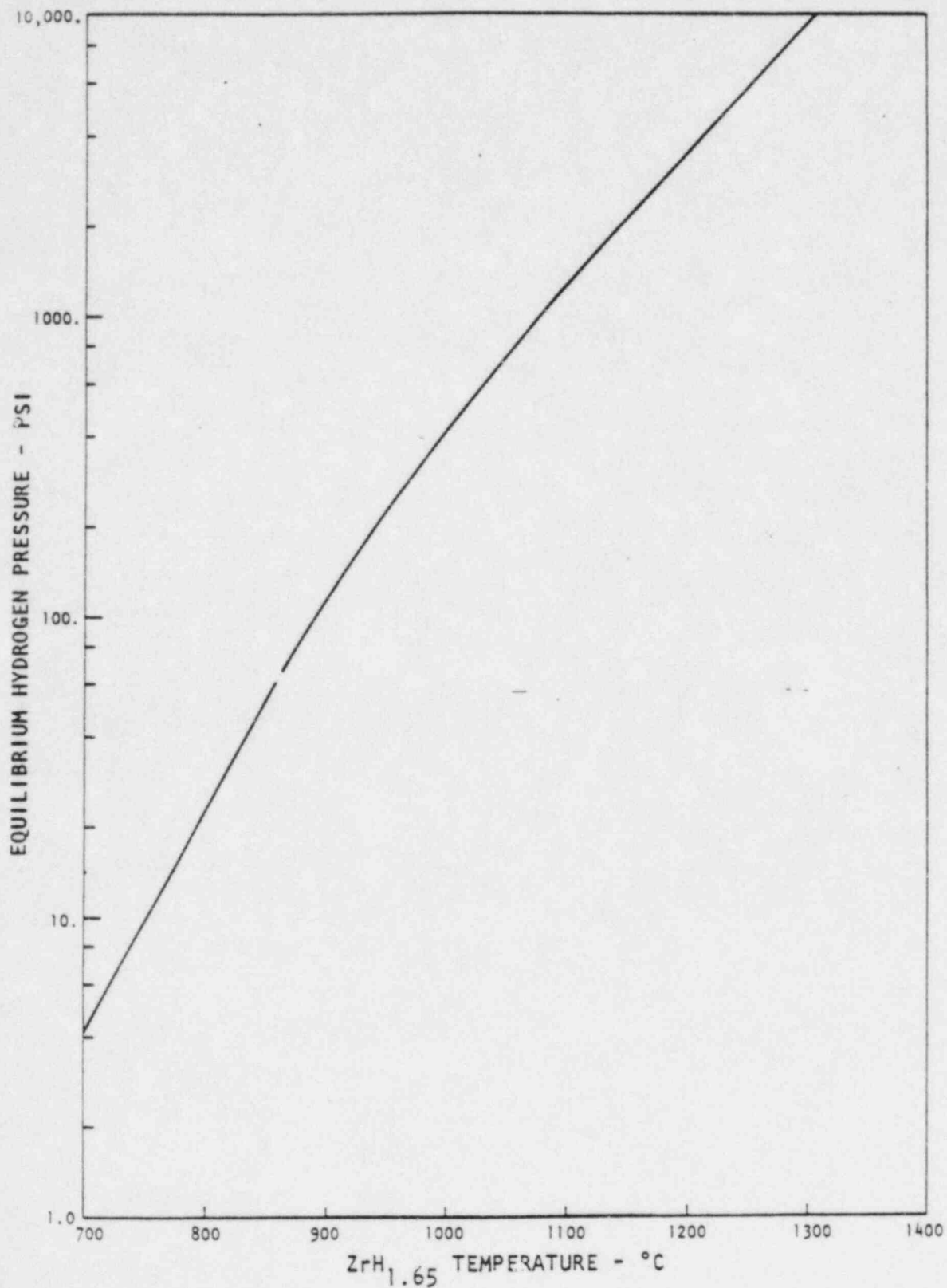
Reactors fueled with TRIGA U-ZrH fuel-moderator elements exhibit a strong prompt negative temperature coefficient of reactivity. For the stainless steel clad U-ZrH_{1.7} fuel, the temperature coefficient is -1.26×10^{-4} $\delta k/k$ per $^{\circ}\text{C}$. There are several factors contributing to the prompt coefficient as noted below:

RELATIVE MAGNITUDE OF CONTRIBUTING COMPONENTS OF THE PROMPT NEGATIVE TEMPERATURE COEFFICIENT OF TRIGA REACTORS

	U-ZrH _{1.0} , Al Clad (%)	U-ZrH _{1.7} , SS Clad (%)
1. <u>Cell</u> increased disadvantage factor with increased fuel temperature leading to a decrease in neutron economy	40	60
2. Irregularities in the fuel lattice due to <u>control rod positions</u> —essentially same effect as 1 above	10	10
3. <u>Doppler broadening</u> of U ²³⁸ resonances—increased resonance capture with increased fuel temperature	20	15
4. <u>Leakage</u> —increased loss of thermal neutrons from the core when the fuel is heated ^a	30	15

^a The low-hydride core is assumed to be reflected by graphite radially, whereas the high hydride core is water reflected radially. The graphite reflector gives ~30% more negative contribution to the leakage component for either core.

[Taken from: General Atomics Report # E-117-855, "The U-ZrH₂ Alloy: Its Properties and Use in TRIGA Fuel," by M.T. Sinnad, General Atomics Corporation, San Diego, CA, GA Project No. 4314, Feb. 1959, page 2-15.]



EL-1174

Fig. 2-9. Equilibrium hydrogen pressure over ZrH_{1.65} versus temperature

ATTACHMENT 6

AFFIDAVIT
OF
JOSEPH A. SHOLTIS, JR.

Joseph A. Sholtis, Jr., being duly sworn according to law, deposes and says:

The Licensee does analyze reactivity transients and a LOCA within sections 6.2.2 and 6.3.3, respectively, of the AFRRRI SAR and fails to obtain conditions necessary to achieve even a single fuel element clad failure. In fact, Intervenor has stated in its response to Licensee's interrogatory #24f that clad failures are not expected during a LOCA unless pulsing operation also takes place concurrently. Moreover, the AFRRRI SAR explicitly addresses and conservatively analyses the consequences of a fuel element clad failure accident and specifically designates such an accident as a DBA. (See sections 6.3.2 and 6.3.4.2 of the AFRRRI SAR.) Although concurrent multiple clad failures are not explicitly addressed in the AFRRRI SAR, due to their extremely low (non-credible) probability, they are nevertheless addressed within section 4.3 of the AFRRRI Reactor Facility Emergency Plan, and the consequences of such multiple clad failure events can readily be obtained simply by multiplying the consequences of a single element failure, from the AFRRRI SAR, by the number of elements presumed involved in the multiple failure event.

Each of the Intervenor's postulated causal mechanisms for a concurrent multiple clad failure accident under this contention is listed below with a brief discussion of why each is considered not credible.

- a. Material Defects: Not credible due to extremely low probability for concurrent failure. Individual clad failures due to material defect are generally considered to occur as random stochastic failures, therefore, two or more such occurrences

concurrently in time are viewed as being non-credible. No concurrent multiple clad failure accident due to material defect has ever occurred in the entire history of TRIGA reactors, and even if such an event were to occur, its consequences would be limited since individual clad failures due to material defect have always occurred early in an element's life so that burnup and the accumulation of fission products within such an element would be limited but certainly well below the equilibrium saturation activity utilized for a single clad failure accident in the AFRRRI SAR.

b. Uncontrolled Power Excursion: Not credible since fuel temperatures and pressures necessary to breach the cladding cannot be attained either via steady-state or pulse operations. Note: The AFRRRI reactor is incapable of firing pulses in rapid succession as Intervenor claims (see Motion for Summary Disposition relating to Contention 9 Accidents IV.) The TRIGA reactor's inherent negative temperature coefficient of reactivity will ensure automatic reactor shutdown before fuel damage results. Even if all of Licensee's authorized k -excess of 5.00 were available and inserted in a step fashion, fuel temperatures and pressures would still remain well below the point where clad failure might be expected. (See section 6.2.2 of the AFRRRI SAR.)

c. LOCA: Not credible since fuel temperatures and pressures necessary to breach the clad cannot be attained. (See Licensee's Application for Amendment to License R-84 in 1964-65, Docket 50-170, where conservative calculations for a LOCA are provided.) Even if it is postulated that pulsing must occur concurrent with a LOCA, clad failure is not credible since pulsing (single or repetitive) cannot be performed during a LOCA. (See Motion for Summary Disposition and my affidavit concerning contention 9. Accidents IV.) (See also section 6.3.3 of the AFRRRI SAR.)

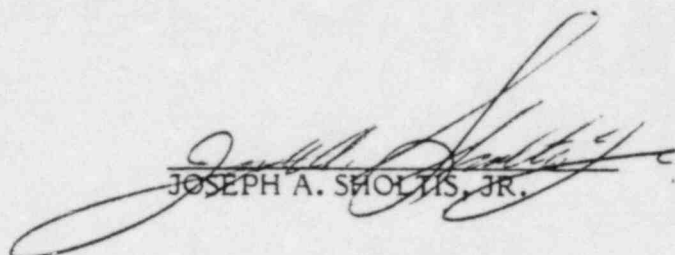
d. Sabotage: Not credible due to extremely low probability. The AFRRI Reactor Physical Security Plan, which is protected from public disclosure, provides information and data to illustrate the protection afforded to detect and respond to sabotage and which, thus, makes such an event non-credible from the standpoint of probability. (See AFRRI Reactor Facility Physical Security Plan.)

e. Aircraft Collision: Not credible due to extreme remoteness of such an event. AFRRI is not beneath any scheduled air traffic route. The vast majority of aircraft crashes occur upon take-off and landing (within a few miles of the airport). AFRRI is not near enough to either National Airport or Dulles Airport to be considered in a high risk area for aircraft crashes. Even if the incredible should occur, any consequent release would be insignificant with respect to the normal consequences of an aircraft crash, such as petroleum fuel fires, structural damage, etc. (See NRC Staff's Safety Evaluation Report on the AFRRI Reactor.)

f. Natural Act-of-God Accidents: Not credible due to extreme remoteness of such an event. Just as for the case of aircraft collision, an Act-of-God event which is adequate to cause multiple clad failures would also produce extensive concurrent collateral damage which would far exceed the consequences of a release. (See NRC Staff's Safety Evaluation Report on the AFRRI Reactor.)

In summary, Licensee submits that multiple clad failures due to the Intervenor's postulated causal mechanisms are either not possible or yielding clad failures or are not likely enough to be considered credible. Licensee has provided, in its SAR and its application for relicensing, adequate assurance that multiple clad failures are not credible events and the

NRC Staff has agreed with the Licensee in this area. (See SER as well as page 53, lines 2 through 9, inclusive, of the transcript of Dr. Stillman's deposition in New York on 18 Dec 1982.)


JOSEPH A. SHOLTIS, JR.

Sworn to and subscribed before
me on this 25th day of Feb, 1983.

my commission expires:
April 8, 1984

Miriam E. Lealey
Notary Public

ATTACHMENT 7

GA-A15384

**TRIGA LOW-ENRICHED URANIUM
FUEL QUENCH TESTS**

**Work Done By:
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**GENERAL ATOMIC PROJECT 4314
JULY 1980**

GENERAL ATOMIC COMPANY

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1. SUMMARY

Quench tests were performed on TRIGA low-enriched uranium (LEU) fuel samples from temperatures ranging from 800° to 1200°C. Fuel samples quenched from 800°, 1000°, 1100°, and 1200°C showed remarkably benign response to the test conditions. Minor cracking occurred in some samples; volume shrinkage, loss of hydrogen, and apparent surface oxidation occurred in all samples. Test results on samples quenched from approximately 1100°C were variable; these variations were at first believed to have been caused by differences in the fuel homogeneity. The results on some samples were benign (minor cracking, volume shrinkage, loss of hydrogen, and surface oxidation), while localized melting occurred on other samples when heated to a measured temperature of 1050°C. The localized melting was caused by eutectics formed from reaction of the Inconel-600 thermocouple sheath with the fuel sample. Samples quenched from 1200°C show variable behavior only because one sample contacted and reacted with the tantalum susceptor originally used. The second sample showed very satisfactory benign behavior.

2. INTRODUCTION

Quench tests from temperatures ranging from 800° to 1200°C were performed on 20% enriched 45U-53Zr-1Er-1H* TRIGA LEU fuel samples to simulate conditions when water ingress occurs upon rupture of the cladding in a fuel rod. The tests were similar to those performed for the same purpose in 1958 on 20% enriched 80-91Zr-1H TRIGA fuel samples and rods. Only minor cracking and surface oxidation occurred in these earlier tests, and the current tests were performed to determine if the change to the new fuel composition would significantly influence fuel behavior under the quench test conditions.

* 45 wt-% uranium, 53 wt-% zirconium, 1 wt-% erbium, 1 wt-% hydrogen.

3. DESCRIPTION OF SAMPLES, EXPERIMENT, AND EQUIPMENT/APPARATUS

The samples used in the quench tests were cut from TRIGA LEU fuel rods in pieces 12.9 mm in diameter and ranging from 12 to 18 mm in length. A hole was drilled into each fuel sample to allow insertion of the 1.6-mm-o.d., Inconel-600-sheathed, MgO-insulated, Chromel-Alumel thermocouple. For the fuel tests at 800° and 1000°C, the sheathed thermocouple was inserted directly in contact with the hole surfaces. When it was found that fuel melting and fuel-thermocouple reactions occurred in tests performed at approximately 1100°C and higher temperatures, a molybdenum cup was inserted in the fuel hole to prevent reaction between the fuel sample and the Inconel 600 thermocouple sheath.

The experimental equipment used is shown schematically in Fig. 1* and in the Fig. 2 photograph. The equipment consists of a vacuum inert atmosphere quartz tube furnace in which a molybdenum (initially tantalum) susceptor is induction-heated by a Tocco 30-kW motor-generator induction unit. (The susceptor material was changed because the tantalum hydrided when the samples released hydrogen; molybdenum does not absorb hydrogen.)

The susceptor radiant heats the fuel sample. The system is twice evacuated to 10^{-5} torr and back-filled to atmospheric pressure with commercial argon prior to heating the sample. When the sample is heated to the desired temperature, valve 2 (Fig. 1) is opened momentarily while the sample is dropped by pushing the sample holder rod through the sliding seal. Typical time at temperature for the sample is 3 min.

The thermocouple was inserted directly into test samples 1 through 5, and an attempt was made to drop the sample. The thermocouples inserted

* All figures and tables are at the back of this report.

In samples 6 and 8, which were not quenched, were heated simultaneously with samples 7 and 9, respectively, which were quenched. This was done to allow temperature to be measured in sibling samples, yet avoid having the thermocouple interfere with the quenching operation.

Immediately after the sample is dropped into the quench chamber bottom, valve 2 is closed to minimize air ingress into the furnace chamber. The atmosphere in the quench water chamber is exhausted through an elephant trunk to an absolute filter hood. Since the fuel sample cools quickly, the experimenter immediately unfastens the quick-connect flange, reaches into the water chamber, and retrieves it.

The furnace and thermocouples were checked using a 12.9-mm-diameter by 12.9-mm-long 316SS cylinder as the test specimen (Fig. 2). The thermocouple junctions were inserted in holes 3.175 and 6.35 mm (axial center of 12.9-mm-diameter sample) from the surface. The type 316SS cylinder was used to minimize effects of possible thermocouple/sample reactions during the checkout.

In the first calibration trial, thermocouple 1 was inserted in the 3.175-mm-deep hole, and thermocouple 2 was inserted in the 6.35-mm-deep hole. For the second calibration trial, the positions of the thermocouples were reversed. The results upon heating in the test apparatus are given in Table 1.

The two thermocouples behaved similarly. Upon heating to approximately 1000°C, the temperature difference between the sample 3.175 mm from the surface and that in the center of the cylinder (6.35 mm from the surface) was approximately 20°C. This outcome indicates that the actual surface temperature is 1020°C when the thermocouple 3.175 mm from the surface shows 1000°C.

4. CHARACTERIZATION OF TRIGA FUEL SAMPLES PRIOR TO AND AFTER TESTING

The experimenter characterized the fuel samples prior to and after testing by photographing their external appearance (Figs. 3 through 20), performing dimensional measurements and weighing them. The dimensional and weight data before and after testing are given in Table 2. The net changes are also listed. Test result details are discussed in Section 5.

5. TEST RESULTS

Quench testing at 800°C (sample 1) and 1000°C (sample 2) went as expected with no unusual occurrences. However, initial tests attempted at 1050°C and higher temperatures (samples 3, 4, and 5), where the Inconel-sheathed thermocouple was in direct contact with the fuel, resulted in localized fuel melting. Samples 3 and 4 were from fuel rod E451R1H, whereas sample 5 and later samples were from fuel rod E451R1L. Remaining tests from sample 6 onward (all from E451R1L) did not result in localized melting. These observations would indicate that the localized melting was the result of fuel reactions with the Inconel 600 thermocouple sheath, rather than inhomogeneities in the fuel. The major constituents of Inconel 600 are nickel, chromium, and iron (72Ni-14-87Cr-6-10Fe). Low-melting eutectics between uranium and the Inconel 600 constituents occur as follows: nickel 740°C, chromium 859°C, and iron 725°C. (See Appendices A, B, and C.) The melting points of the eutectics between zirconium and the Inconel 600 constituents are as follows: nickel 961°C, chromium 1300°C, and iron 934°C. (See Appendices D, E, and F.)

5.1. 800°C FUEL SAMPLE QUENCH TEST RESULTS

The 800°C fuel quench test (sample 1) resulted in a weight loss of 0.115 g from a total weight of 19.9152 g. This constitutes a 0.57 wt % loss. There appeared to be very little test-related surface oxidation on the sample, so it may be assumed that the weight loss was due to release of hydrogen. The fuel initially contained 0.88 wt % hydrogen. Hence, 0.57/0.88 wt % or 64.8% of the initial hydrogen present in the sample was lost during the test. The sample showed only a minor radial crack and volume shrinkage (approximately 1% differential volume/volume) that could be attributable to the loss in hydrogen.

Some positive pressure change, i.e., 13.788 kPa, typically occurred in the furnace atmosphere during the treatment of the samples, indicating outgassing and release of hydrogen. The pressure change was slightly larger for the samples subsequently heated to higher temperatures. Some of the pressure change was undoubtedly also due to temperature increase of the argon atmosphere in the furnace.

5.2. 1000°C FUEL SAMPLE QUENCH TEST RESULTS

The 1000°C fuel quench test (sample 2) resulted in a weight loss of 0.0714 g from a total weight of 15.954 g. This constitutes a 0.45 wt % loss. Some surface oxidation appears to have occurred, with a weight gain from oxidation offsetting some of the hydrogen weight loss. No post-test visual surface cracks were found on the sample. The volume shrinkage, approximately 4.6%, appears to indicate more hydrogen loss and sintering than found in the sample quenched from 800°C, even though the overall fractional weight loss was less.

5.3. 1100°C FUEL SAMPLE QUENCH TEST RESULTS

The 1100°C quench fuel test (sample 7) resulted in a weight loss of 0.7764 g from a total initial weight of 18.3849 g. This constitutes a 4.23 wt % loss. Since the original sample contained only 0.88 wt % hydrogen, even if all of the hydrogen in the samples were removed during heating to 1100°C, an additional 3.35 wt % loss could possibly be attributed to other constituents having been removed, possibly as oxides. However, since the sample quenched from 1200°C showed a lower weight loss, this is not highly probable (see Section 5.4). Although the sample had developed a radial crack on one end, there was very minimal evidence of chipping or other removal of solid material resulting from heating to 1100°C or from dropping into the quench chamber.

5.4. 1200°C FUEL SAMPLE QUENCH TEST RESULTS

The 1200°C fuel quench test (sample 9) resulted in a weight loss of 0.0600 g from a total initial weight of 13.9545 g. This constitutes a 0.375 wt % loss. Since the original sample contained 0.88% hydrogen, and it is expected that virtually all of the hydrogen would have been released upon heating to 1200°C, the hydrogen weight loss was compensated in part by a weight gain due to sample surface oxidation. Other than minor surface oxidation, the sample showed only minor markings where the sample was supported with a molybdenum wire and no other adverse effects in appearance. The volume shrinkage of 5.2% is comparable to that observed on sample 2, which was quenched from 1000°C.

5.5. MICROPROBE ANALYSES

Microprobe analysis indicated that the localized melting on sample 4 surfaces, which occurred wherever the Inconel 600 thermocouple was in direct contact with the fuel at 1150°C, had the following characteristics:

1. The once-molten beads or blisters were of a high uranium content and in some cases were nearly pure uranium. The pits or pockets near the once-molten beads or blisters had a high uranium content, while the matrix surrounding them had a high zirconium content. These results appear to indicate general high uranium content microinhomogeneities with a 10 to 150 μm^* size range in the fuel, probably increased by the formation of eutectics with the thermocouple sheath, as described in No. 3 below.
2. In regions near the thermocouple location, in what appeared to be the reaction zone between the Inconel 600 sheath and the fuel, uranium-chromium plate crystals were found, and iron and magnesium were also present. These results indicate that low-melting eutectics formed between the uranium in the fuel and the constituents of the Inconel 600 sheath of the thermocouple and the MgO insulation between the sheath and the thermocouple.

* μm = micrometer = 10^{-6} meters = 1 micron.

3. It appears that the low-melting eutectics initiated the localized melting by general surface melting of the samples. In surface regions distant from the source of chromium, iron, and nickel (the Inconel 600 thermocouple sheath) melting also occurred, but the high vapor pressure of these constituents would have caused them to disappear by vaporization during heating, leaving only the high uranium content to be detected by the microprobe analysis. The grain size in the fuel is significantly larger throughout the fuel rod where low melting temperature eutectics have formed versus where no uranium melting has occurred. The higher uranium content regions are due to the microinhomogeneities which are smaller than the grain size in the fuel as fabricated (grain size = 40 to 120 μm). Studies by Baldwin* have shown that microinhomogeneities are characterized by uranium-rich grain boundaries surrounding grains with relatively uniform uranium and zirconium content. This type of uranium distribution provides a beneficial effect on fission gas release rates (release rates are lower by one to two orders of magnitude) as compared to fuel in which the uranium is distributed uniformly as a finer dispersion. In the regions where melting occurred, the microinhomogeneities were found within the grains as well as in the grain boundaries. The grain size ranged up to 150 μm as compared to an as-fabricated grain size of 40 to 120 μm .

Sample 3, which had been heated to 1200°C in contact with a thermocouple, was sectioned longitudinally so that material away from the surface could be analyzed. Microprobe analyses showed that, in regions away from the fuel surface and the zone of reaction between the fuel and the thermocouple sheath, the primary constituents were uranium and zirconium, as expected. The concentration of uranium varied locally (6% to 80%), but

* General Atomic private communication.

It was generally from about 40 wt % to as high as 80 wt % uranium (the nominal average composition is 45 wt % uranium). The microprobe electron beam size employed was 5 by 50 μm , while single grains in this sample were as large as 150 μm in diameter. Therefore, the variation in composition observed was very local and is classified as microinhomogeneities rather than general inhomogeneities. Away from the reaction zone in the sample, the uranium to zirconium ratio was approximately unity. Silicon from an unknown source was found on the surfaces of the hole in which the thermocouple was located, and this silicon reacted with the fuel.

TRIGA Fabrication reports that the fuel specimen was sectioned with an alumina (Al_2O_3) abrasive wheel. It is difficult to understand why silicon rather than aluminum was found on the surfaces of the hole if the cutting wheel was the source of the silicon.

Microprobe analyses were performed on the surfaces of the above samples in regions away from where the thermocouple reactions occurred. Microprobe analyses were also performed on the surfaces of several samples in which little or no melting occurred during heating prior to quenching, as well as on two archive samples which represent the fuel in the as-fabricated condition. The microprobe results for the surfaces of all of the samples analyzed are summarized in Table 3.

The amounts of uranium and zirconium shown do not add up to 100% because (1) the microprobe analyses average the uranium and zirconium content over a 50- μm -diameter spot, and (2) the correction factor for the X-ray absorption by the matrix has not been applied (the absorption factor was calibrated for 50% uranium, 50% zirconium, but varies considerably with varying amounts of uranium and zirconium). (The details of microprobe analyses are given in Appendices G and H).

5.6. HYDROGEN ANALYSES

Hydrogen analyses were performed on samples 1 through 5, 7, and 9 after quench or heating to test temperature and on two as-fabricated

control samples E451R1L and E451R1H. The analyses were performed by the vacuum fusion method. The analyses accuracy is estimated to be $\pm 15\%$ by M. Blatt, who performed the tests. The results are given in Table 4.

5.7. METALLOGRAPHIC EXAMINATION

5.7.1. As-Fabricated Control Specimens

The fuel rods were fabricated by casting, zone melting, and hydriding the 45 wt % U-Zr alloy. The microstructures of the specimens are typical of hydrided dendritic structures. There are variations in composition and grain size on a microscopic scale, both axially and radially. The arrays of fine pores at grain boundaries and within grains and the general features of the microstructures may be related to the recrystallization, phase changes, and grain boundary segregation during hydriding. The phases that are present include uranium, zirconium hydride, and zirconium and uranium carbides. Variations in rates of solidification and of hydriding govern the types of microstructural features that are observed. (See Figs. 21 and 22.)

5.7.2. Specimens Quenched From 800°C

There is little change in the microstructure of the specimens quenched from 800°C compared to that of the as-fabricated specimens. A slight difference in structure in the surface area may be related to oxidation and some loss of hydrogen from the surface region. (See Fig. 23.)

5.7.3. Specimens Quenched From 1000°C

The effects are similar to those observed in the specimens quenched from 800°C. (See Fig. 24.)

5.7.4. Specimens Quenched From 1050°C

There is some enhancement of the changes in microstructure observed at 800° and 1000°C, particularly in the homogenization and in the number of fine voids which may be associated with increased loss of hydrogen. (See Fig. 25.) The homogenization referred to here is related to the composition or local uranium and zirconium content. Upon heating at temperatures >800°C, the diffusion of the constituents will tend to remove concentration gradients and composition differences in the fuel. Also, long-term heating at high temperatures will tend to promote grain growth, and a grain size larger than that in the as-fabricated condition will result.

5.7.5. Specimens Quenched From 1100°C

The effects are similar to those observed in the 1050°C quench with further enhancement of the homogenization process. (See Fig. 26.)

5.7.6. Specimens Quenched From 1150°C

There is markedly increased homogenization, although the grain boundaries consisting of fine arrays of voids are still in evidence. Reaction of the fuel with the Inconel-600 thermocouple sheath resulted in the formation of eutectics. (See Fig. 27.)

5.7.7. Specimens Quenched From 1200°C

Figure 28 shows the microstructures of specimens which reacted with the Inconel-600 thermocouple sheath with the formation of eutectics. Figure 29 shows the microstructures of specimens which were isolated from the thermocouple sheath by means of molybdenum foil cups. The eutectic evidently penetrated rapidly along grain boundaries in the specimens which

were in contact with the Inconel-600 thermocouple sheaths. The specimens which were protected from the thermocouple sheath have a significantly more homogeneous microstructure, although the grain boundaries with arrays of fine voids are still in evidence. There has been a large loss of hydrogen, particularly from the surface regions where small voids are visible in the grains. Surface oxidation is also evident, with an oxide film approximately 6 microns thick.

6. CONCLUSIONS

The following conclusions can be drawn from quench tests on TRIGA LEU fuel samples from temperatures ranging from 800° to 1200°C.

1. All samples in which there was no contact with the Inconel 600 thermocouple sheath survived the tests in excellent condition. All samples quenched from 800° and 1100°C had minor cracking.
2. For those samples in which the fuel was in contact with the Inconel-600 thermocouple sheath, no obvious reactions occurred to 1000°C. Above approximately 1050°C, eutectics of uranium with nickel, chromium with iron, and possibly zirconium with nickel and iron formed, which resulted in localized melting of the surface of the fuel sample. These results indicate satisfactory behavior of TRIGA fuel for temperatures to at least 1200°C. Under conditions where the clad temperature can approach the fuel temperature for several minutes (which may allow formation of eutectics with the clad), the results indicate satisfactory behavior to about 1050°C. This is still about 50° to 100°C higher than the temperature at which internal hydrogen pressure is expected to rupture the clad, should the clad temperature approach that of the fuel.
3. Inconel sheathed thermocouples are used to monitor temperatures in both standard and TRIGA LEU fuel. No problems have been observed from their presence. The lowest temperature eutectic in the system is iron with uranium, which occurs at 725°C. At temperatures <725°C, the diffusion rates are very low and no eutectics form. For pulsing reactors where in the fuel is pulsed to >1050°C, the time at temperature is very short, a small

fraction of a second. The time is so short that there will not be sufficient diffusion to cause eutectics to form between the fuel and the Inconel sheath. Under design operating conditions, the presence of the Inconel-sheathed thermocouples in the TRIGA fuel has been and is expected to continue to be satisfactory.

FUEL ELEMENTS FOR PULSED TRIGA[®] RESEARCH REACTORS

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TRIGA fuel was developed around the concept of inherent safety. A core composition was sought that had a large prompt negative temperature coefficient of reactivity such that if all the available excess reactivity were suddenly inserted into the core, the resulting fuel temperature would automatically cause the power excursion to terminate before any core damage resulted. Experiments have demonstrated that zirconium hydride possesses a basic neutron-spectrum-hardening mechanism to produce the desired characteristic. Additional advantages include the facts that ZrH has a good heat capacity, that it results in relatively small core sizes and high flux values due to the high hydrogen content, that it has excellent fission-product retentivity and high chemical inertness in water at temperatures up to 100°C, and that it can be used effectively in a rugged fuel element size.

Tens of thousands of routine pulses to the range of 500 to 800°C peak fuel temperatures have been performed with TRIGA fuel, and a core was pulse-heated to peak fuel temperatures in excess of 1100°C for hundreds of pulses before a few elements exceeded the conservative tolerances on dimensional change.

INTRODUCTION

The development of uranium-zirconium hydride fuels for the TRIGA pulsing reactor has been underway at General Atomic since 1957 (Refs. 1 through 5). Over 6000 fuel elements of 7 distinct types have been fabricated for the 55 TRIGA research reactors under construction or already in

operation. The basic characteristics of the TRIGA research reactors are:

1. The use of homogeneous U-Zr hydride solid fuel-moderator elements with a large prompt negative temperature coefficient of reactivity.
2. Light-water open-pool design, with natural convection cooling up to 2 MW and forced cooling above this level.
3. Power pulsing capability, licensed to pulse routinely up to a reactivity insertion of 3.2% $\delta k/k$ (\$4.60) and peak power level of 6500 MW, providing an integrated neutron flux of up to $\sim 10^{15}$ n/cm² per pulse. A special annular core pulsing (ACPR) TRIGA reactor at Sandia Corporation achieves a peak pulse of 12 000 MW (Refs. 4, 5, and 6). The ACPR has a large central irradiation space for pulse-testing reactor fuels, materials, and equipment.

A typical ACPR fuel-moderator element is shown in Fig. 1. The active section is 15 in. long and 1.4 in. in diameter and contains ~ 12 wt% uranium enriched to 20% in ²³⁵U. To facilitate hydriding, a 0.25-in.-diam hole is drilled through the center of the active section; a zirconium rod is inserted in this hole after hydriding is complete. Graphite cylinders ~ 3.4 in. long and 1.4 in. in diameter act as top and bottom reflectors.

The active fuel section and the top and bottom graphite cylinders are contained in a 0.02-in.-thick stainless-steel clad. (The clad is provided with internal dimples that act as spacers to ensure a thermal gap of ~ 0.010 in. between the fuel meat and the clad.) The stainless-steel cladding is welded to the top and bottom end fitting, positions the top of the fuel element in the top grid plate, and is fluted to provide passage for natural

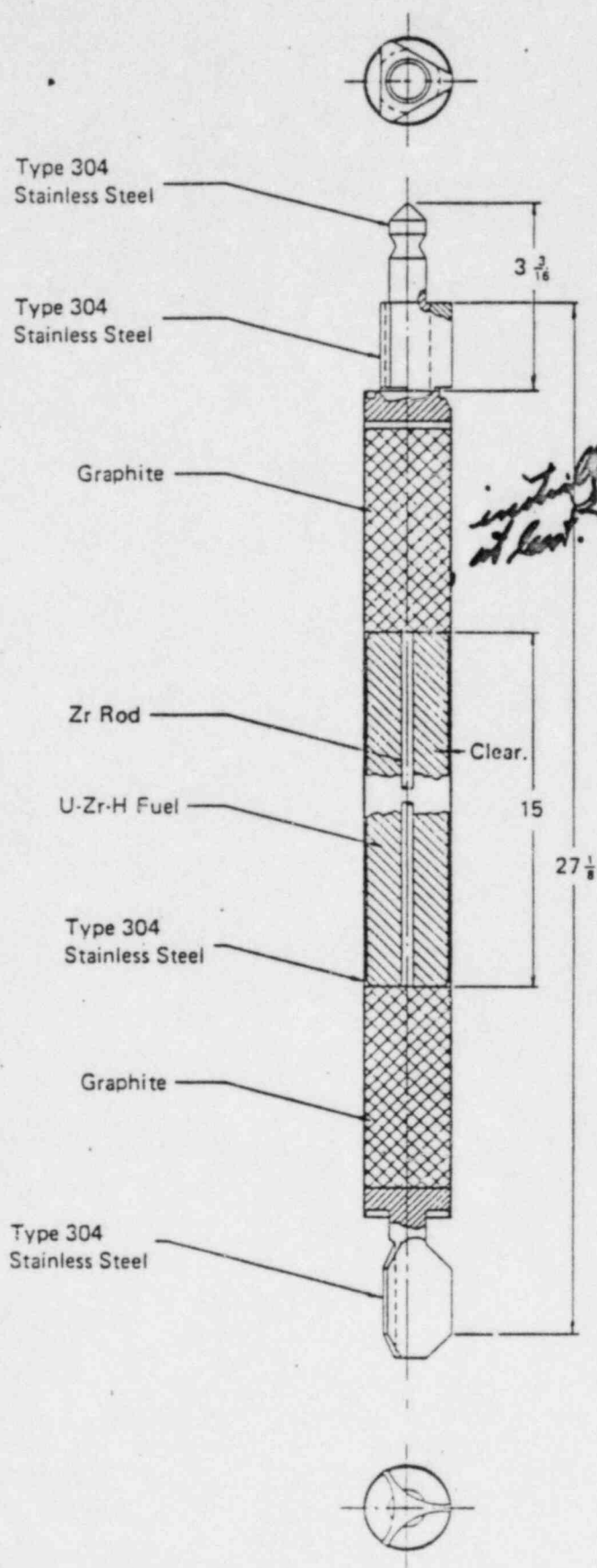


Fig. 1. Typical TRIGA fuel element.

convection cooling-water flow upward through the grid. The bottom end fitting, which is also fluted to provide water flow passages, rests and is centered on the chamfered hole in the bottom grid plate. The bottom grid plate supports the weight of the element, which is 7.5 lb, and the ^{235}U content is ~54 g.

The standard TRIGA fuel has a cold gap of only 1 or 2 mils between the fuel and clad, is slip-fit together and contains 8.5 to 12 wt% uranium as a fine metallic dispersion in a zirconium hydride matrix. The H/Zr ratio is 1.60 [in the face-centered cubic (fcc) delta phase]. The equilibrium hydrogen dissociation pressure is governed by the composition and temperature. For ZrH_x , the equilibrium hydrogen pressure is 1 atm at 760°C. The single-phase high-hydride composition eliminates the problems of density changes associated with phase changes and thermal diffusion of the hydrogen. The recently developed Fuel Lifetime Improvement Program (FLIP) fuel contains up to 3.0% erbium as a burnable poison to increase the core lifetime in the higher power (1- to 14-MW) TRIGA reactors.^{7,8} (Cores with steady-state power levels above 2 MW are not pulsing cores.) The calculated core lifetime with FLIP fuel in the 2-MW TRIGA is ~9 MW-yr. Over 25 000 pulses have been performed with the TRIGA fuel elements at General Atomic, with fuel temperatures reaching peaks of ~1150°C.

TRIGA fuel was developed around the concept of inherent safety. A core composition was sought that had a large prompt negative temperature coefficient of reactivity such that if all the available excess reactivity were suddenly inserted into the core, the resulting fuel temperature would automatically cause the power excursion to terminate before any core damage resulted. Experiments then in progress demonstrated that zirconium hydride possesses a basic mechanism to produce the desired characteristic. Additional advantages included the facts that ZrH has a good heat capacity, that it results in relatively small core sizes and high flux values due to the high hydrogen content, and that it can be used effectively in a rugged fuel element size.

Current routine power levels for TRIGA reactors (1 to 2 MW) require operational excess reactivities that cannot be instantaneously inserted into the core with complete safety. However, the safety of these systems has not been compromised because no single control rod or experiment is worth the reactivity necessary to reach an unsafe level. The ACPR, which is optimized for maximum pulsing performance, requires the precise timing of the removal of 3 to 5 pulse rods to produce the operational pulse performance.

The characteristics of the fuel and the design and operational experience with pulsed TRIGA reactors are described in this paper.

PHASE SYSTEMS AND DISSOCIATION PRESSURES

The ZrH and U-ZrH systems are essentially simple eutectoids,⁹ containing at least four separate hydride phases in addition to the zirconium and uranium allotropes (Fig. 2). The hydride phases consist of the following:

1. Alpha phase—a low-temperature terminal solid solution of hydrogen in the hexagonal close-packed alpha-zirconium lattice.
2. Beta phase—a solid solution of hydrogen dissolved in the high-temperature body-centered cubic zirconium phase.
3. Delta phase—an fcc hydride phase (a delta-prime phase has also been reported, formed below 240°C from the delta).
4. Epsilon phase—a face-centered tetragonal (fct) hydride phase with the ratio $c/a < 1$, extending beyond the delta phase to ZrH_2 . The epsilon phase is not a true equilibrium phase and forms from the delta by a martensitic reaction. It appears as a banded twin structure.

When uranium is present, it appears to be partially rejected from solution during the hydriding process. The uranium rejected is present as a fine uniform dispersion. The effect of the uranium addition on the ZrH system is to shift all the phase boundaries of the ZrH diagram to slightly lower temperatures. For example, the eutectoid temperature is lowered from 547 to 541°C. No new phases and no uranium hydride have been detected.¹⁰ At rather high uranium contents (25 to 50 wt%), the behavior with hydrogen was found to be a breakdown of the intermetallic alloy. The zirconium reacted with the hydrogen, giving polyphase regions of uranium, zirconium, and zirconium hydride phases, mainly the cubic delta hydride. The phase boundaries of the ZrH diagram were relatively unaffected in the region of high hydrogen content, but the alpha and beta phases were markedly shifted. The main effect of the addition of uranium in the low hydrogen content region was to considerably increase the range of the alpha phase. Uranium hydride phases were not observed.

There is no generally accepted theoretical description of the structure of metal hydrides.¹¹ At present, two quite different theories are used to discuss metal hydrides. In one, the hydrogen is regarded as losing its electron to the conduction

band of the metal structure, and as being present in the lattice as H^+ . This theory describes the transition metal hydrides as metallic or as alloys. The alternative theory considers that the hydrogen atom acquires an electron from the conduction band and is present as H^- . The depleted conduction band remains to give residual metallic bonding in the hydride and to account for the metallic properties. This theory describes the hydrides as ionic. It is possible that covalent bonding could be introduced into either theory, although few attempts have been made to do so. In any case, the small hydrogen atom would be expected to enter the tetrahedral sites in the usually close-packed metal structure. Nevertheless, most hydrides do not have their metal atoms in the same positions as in the parent metal. The solubility of hydrogen in zirconium above the eutectoid temperature was found to be increased by the presence of beta-stabilizing elements and decreased by alpha-stabilizers.

The rates of hydriding and dehydriding of zirconium are markedly influenced (reduced) by the presence of surface oxide or nitride films. The surface films will, therefore, affect the measured hydrogen dissociation pressures unless precautions are taken to eliminate these films.

The hydrogen dissociation pressures of zirconium hydrides and of U-ZrH have been measured.¹² The concentration of hydrogen is generally reported in terms of either weight percent or atoms of hydrogen per cm^3 of fuel (N_H). The equilibrium dissociation pressures in the ZrH system are given in Fig. 3. In the delta region,¹² the dissociation pressure equilibria of the zirconium-hydrogen binary can be expressed in terms of composition and temperature by the relation

$$\log P = K_1 + \frac{K_2 \times 10^3}{T}$$

where

$$K_1 = -3.8415 + 38.6433X - 34.2639X^2 + 9.2821X^3$$

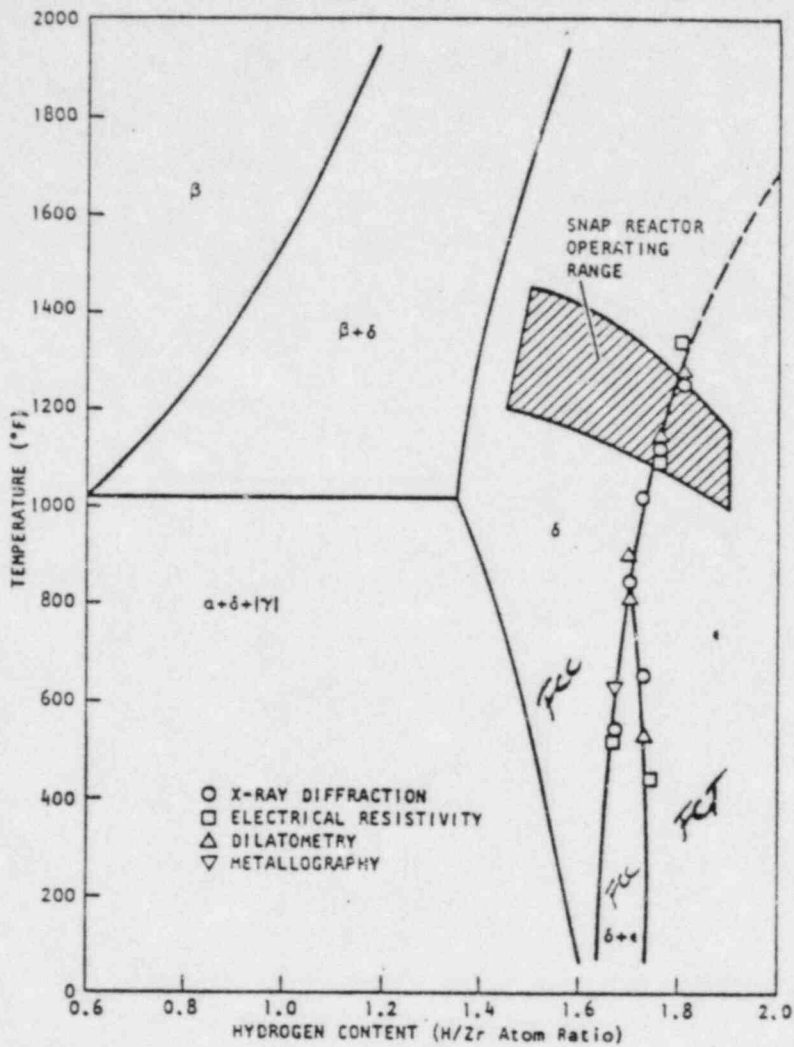
$$K_2 = -31.2982 + 23.5741X - 6.0280X^2$$

P = pressure, atm

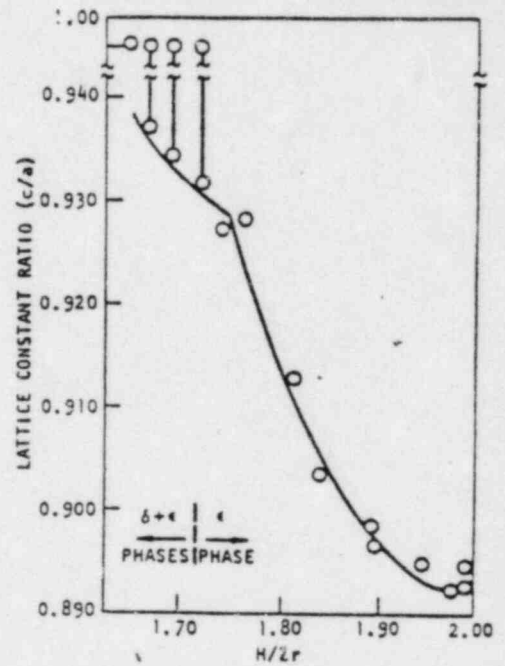
T = temperature, K

X = hydrogen-to-zirconium atom ratio.

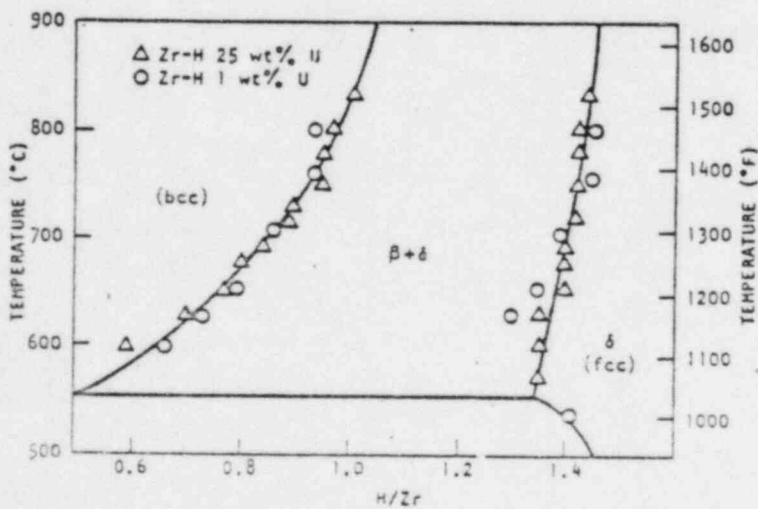
The heat of solution of hydrogen in the delta hydrided phase decreases with increasing solute concentration, from -46.3K cal/mole, in delta of composition $ZrH_{1.4}$, to -37.7K cal/mole, in epsilon of composition $ZrH_{1.5}$ (Ref. 12). It is significant that no discontinuity in the function is in evidence throughout the entire delta-to-epsilon composition range, involving an H/Zr composition range of



(a) ZIRCONIUM-HYDROGEN PHASE DIAGRAM



(c) VARIATION OF DEGREE OF TETROGONALITY OF ϵ HYDRIDE WITH HYDROGEN CONTENT



(b) δ AND $(\delta+\beta)$ ZIRCONIUM HYDRIDE PHASE BOUNDARY

Fig. 2. Zirconium-hydrogen phase relationships (from Ref. 3).

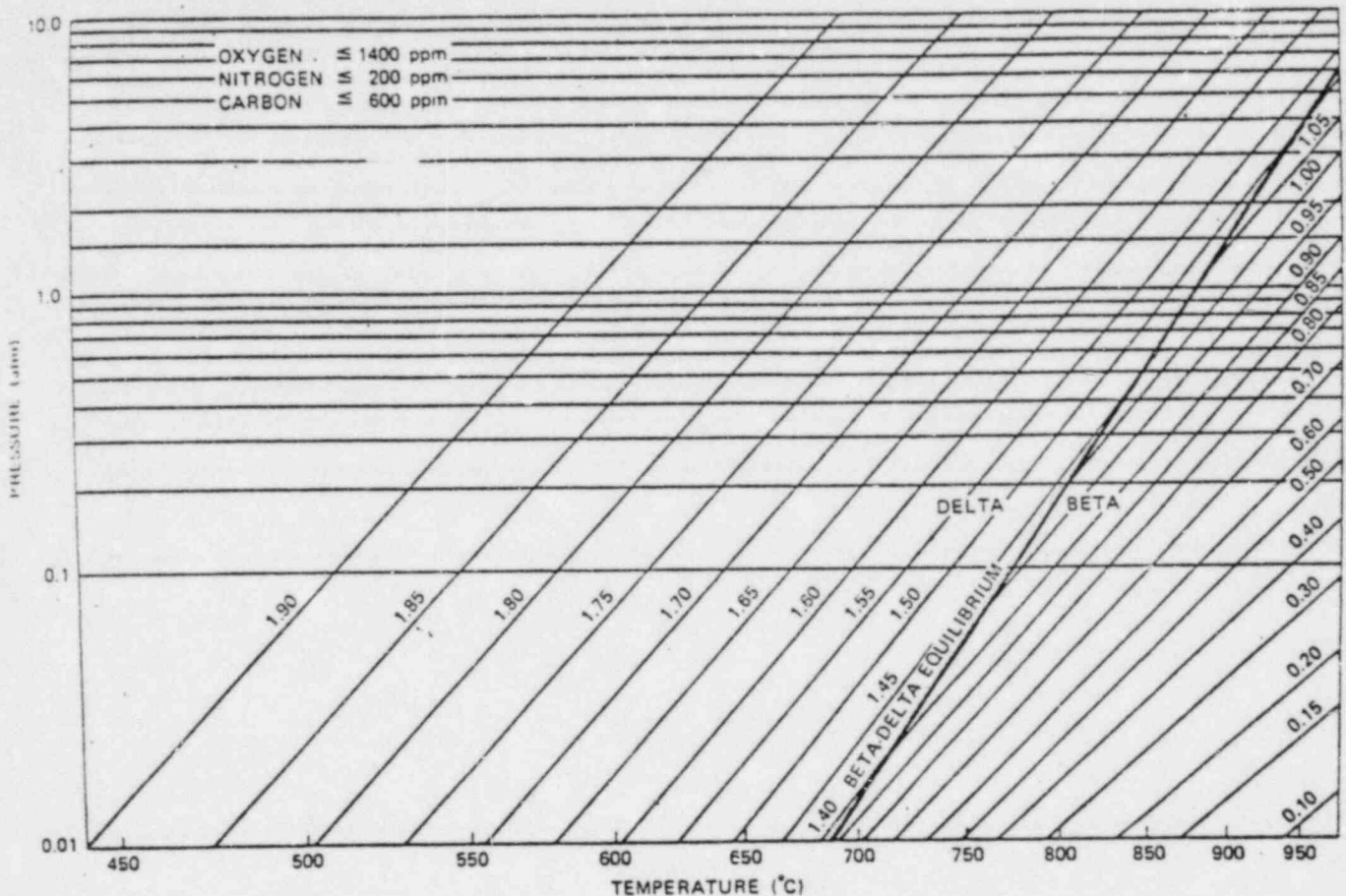


Fig. 3. Dissociation pressure isochores of zirconium hydride (expressed as H/Zr atom ratios) (from Ref. 9).

~1.4 to 1.9. This is compatible with the transition from fcc-delta to fct-epsilon, involving a continuous anisotropic expansion of the cubic phase. The isochores of the delta-epsilon regions of the ZrH system exhibit a progressively increasing change in spacing with increasing hydrogen concentration. Any deviation from this type of progression is attributed to significant contamination of the binary with oxygen, nitrogen, carbon, etc., to form a ternary or higher-order alloy system.

The higher-hydride compositions (H/Zr > 1.5) are single-phase (delta or epsilon) and are not subject to thermal phase separation or thermal cycling. For a composition of about ZrH_{1.5}, the equilibrium hydrogen dissociation pressure is 1 atm at ~760°C. This allows considerable variation in fuel central temperatures without building up high internal gas pressures in the fuel element. The absence of a second phase in the higher hydrides eliminates the problem of large volume changes associated with phase transformations at 740°C in the lower hydride compositions. Similarly, the absence of significant thermal diffusion of hydrogen in the higher hydrides precludes concomitant volume changes and cracking. The clad

material of stainless-steel or nickel alloys will provide a satisfactory diffusion barrier to hydrogen at long-term (several years) sustained cladding temperatures below ~300°C in a water or steam environment.

The equilibrium dissociation pressures in the H/Zr composition range of 1.4 to 1.7 at temperatures up to 1300°C have been measured.² The results for an H/Zr range of 1.55 to 1.7 agree closely with the values obtained from extrapolation of the reported data that extend to 950°C. However, the data for an H/Zr range of 1.4 to 1.5 indicate that the hydrogen dissociation pressures for these compositions are considerably lower than the values extrapolated from the temperatures below 950°C, probably as a result of phase changes at the elevated temperatures. For example, at the H/Zr ratio of 1.5, the measured dissociation pressure at 1100°C is 7.7 atm versus the extrapolated value of 25.2 atm, and at 1200°C is 11.5 atm measured versus 70 atm extrapolated.

The influence of carbon on the dissociation pressures of hydrogen in carbon-modified U-ZrH fuels has been measured.^{7,13} The dissociation pressures were found to be predictably higher

units, at least.

than the dissociation pressures of the carbon-free hydrides. The hydrogen dissociation pressures are expressed as a function of temperature and composition:

$$P = K \exp(-\Delta H_a/RT) ,$$

where the value of K is governed by composition. The carbon associates with zirconium on a 1-to-1 ratio.

All available evidence indicates that the addition of erbium to the U-ZrH introduces no deleterious effects to the fuel. Erbium has a high boiling point and a relatively low vapor pressure so that it can be melted into the uranium-zirconium uniformly. The erbium is incorporated into the fuel during the melting process. All the analyses that have been made on the alloy show that the erbium is dispersed uniformly, as is the uranium. Erbium is a metal and forms a metallic solution with the uranium-zirconium; thus there is no reason to believe that there will be any segregation of the erbium. Erbium forms a stable hydride (as stable as zirconium hydride), which also indicates that the erbium will remain uniformly dispersed through the alloy. Also, since neutron capture in erbium is an $n-\gamma$ reaction, there are no recoil products.

The erbium cannot migrate or segregate in the fuel at the temperatures and times involved since the diffusion rates are much too low. Inter-metallic diffusion rates follow an exponential relationship with temperature and are extremely low at the operating temperatures for this type of alloy. Thus, with a conservative diffusion coefficient of 10^{-13} cm²/sec at 800°C, the diffusion distance would be ~0.1 mm/yr. Hence, there could not be any significant migration during the lifetime of the fuel.

MIGRATION OF HYDROGEN UNDER THERMAL GRADIENTS

Fuel element operation in a reactor is not isothermal, and hydrogen migrates to colder temperature regions from high-temperature regions. The equilibrium dissociation pressure obtained when the redistribution is complete is lower than the dissociation pressure before redistribution. The dimensional changes of (ue) rods due to hydrogen migration are of minor importance in the delta and epsilon phases. In the SNAP-10A reactor, the small amount of hydrogen redistribution in the high hydride was found to be determined by temperature gradients within the elements.

The results of studies at General Atomic on thermal migration of hydrogen in U-ZrH fuel have

been described.¹⁴ No significant hydrogen redistribution was observed in the delta- or epsilon-phase hydrides. In the lower hydrides, however, extensive migration of hydrogen took place.

The kinetics of hydrogen migration in delta zirconium hydride have been measured in the temperature range of 650 to 800°C (Ref. 15). The hydrogen absorption follows a parabolic time law and the rate constant is proportional to the concentration difference and to the square root of the diffusion constant. The temperature dependence of diffusion is given by

$$D = 0.25 \exp[(-17800/RT)]$$

The diffusion of hydrogen in zirconium hydride was found to be independent of concentration.

PHYSICAL, MECHANICAL, AND CORROSION PROPERTIES

The density of U-ZrH decreases with an increase in the hydrogen content, as shown in Fig. 4 (Ref. 16). The density change is quite high (15%) up to the delta phase (H/Zr = 1.5) and then changes little with further increases in hydrogen.

The thermal conductivity measurements have been made over a range of temperatures.¹⁶ A problem in carrying out these measurements by conventional methods is the disturbing effect of hydrogen migration under the thermal gradients imposed on the specimens during the experiments. This has been minimized by using a short pulse-heating technique to determine the thermal diffusivity, and hence permitting calculation of the thermal conductivity. A value of $0.042 + 1.79 \times 10^{-5} T$ cal/sec-cm-°C is used for the thermal conductivity for TRIGA design calculations.

The mechanical properties of U-ZrH are difficult to measure because of its brittle nature. However, at elevated temperatures it exhibits significant ductility and creep deformation. The creep strength is markedly influenced by the structure, as shown in Fig. 5 (Refs. 18 and 19). The beta phase has a much lower creep strength than the delta phase. This is an important factor in the relatively greater irradiation stability of the delta phase at high temperatures.

The hydride fuel has excellent corrosion resistance in water. Bare fuel specimens have been subjected to a pressurized water environment at 570°F and 1230 psi during a 400-h period in an autoclave.²⁰ The average corrosion rate was 350 mg/cm²-month weight gain, accompanied by a conversion of the surface layer of the hydride to an adherent oxide film. The maximum extent of corrosion penetration after 400 h was <2 mil.

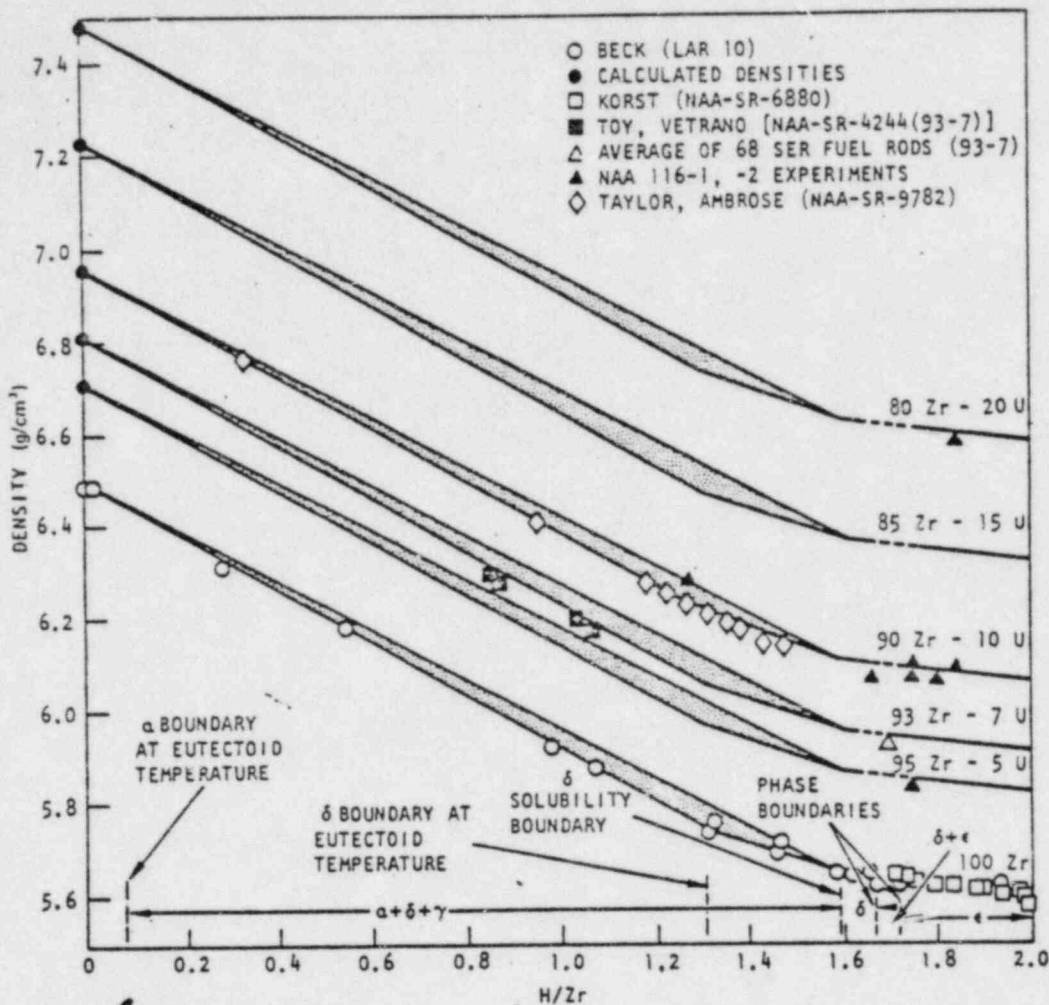


Fig. 4. Density diagram (from Ref. 16).

i.e. chemical reactivity.

Experiments carried out at General Atomic show that the zirconium hydride systems have a relatively low reactivity in water, steam, and air at temperatures up to 850°C. These tests have involved the quenching in water of U-ZrH specimens after heating to as high as 850°C.

IRRADIATION EFFECTS

The U.S. Atomic Energy Commission (AEC) set a limit of 2.54 mm (0.1 in.) for longitudinal growth of fuel elements for all pulsing reactors. Nevertheless, fuel elements have operated intact for long periods in TRIGA reactors with cladding elongations of up to 36 mm (Yugoslavia, non-pulsing TRIGA). A mechanical ratcheting mechanism caused these unusual elongations, which can be eliminated by suitable fuel element design.

Burnups of up to ~0.52 total metal at.% (75% burnup of the ²³⁵U) have been attained successfully with TRIGA fuel elements. The results of exten-

sive irradiation tests in the SNAP reactor program have led to empirical correlations between swelling under steady-state operation and the important variables of temperature, fuel composition, burnup, neutron flux, and fluence. The offset growth during early life (up to ~0.1 metal at.% burnup) is ascribed to the vacancy-condensation-type growth phenomenon. Future developments in the pulsing U-Zr hydride fuels will include studies of the influence of high burnup at steady-state power on the behavior of this fuel under pulsing conditions.

Instrumented pulsing fuel elements have been fabricated to determine the temperature distributions in the fuels and claddings and to record the gas pressures in the fuel elements.

In the ACPR fuel elements a small gas gap (375 μm, 0.015 in.), provided by means of dimples in the cladding, introduces a thermal resistance to control heat flow rates from the fuel immediately after pulsed operation and to prevent film boiling. The fuel elements can be pulsed to temperatures

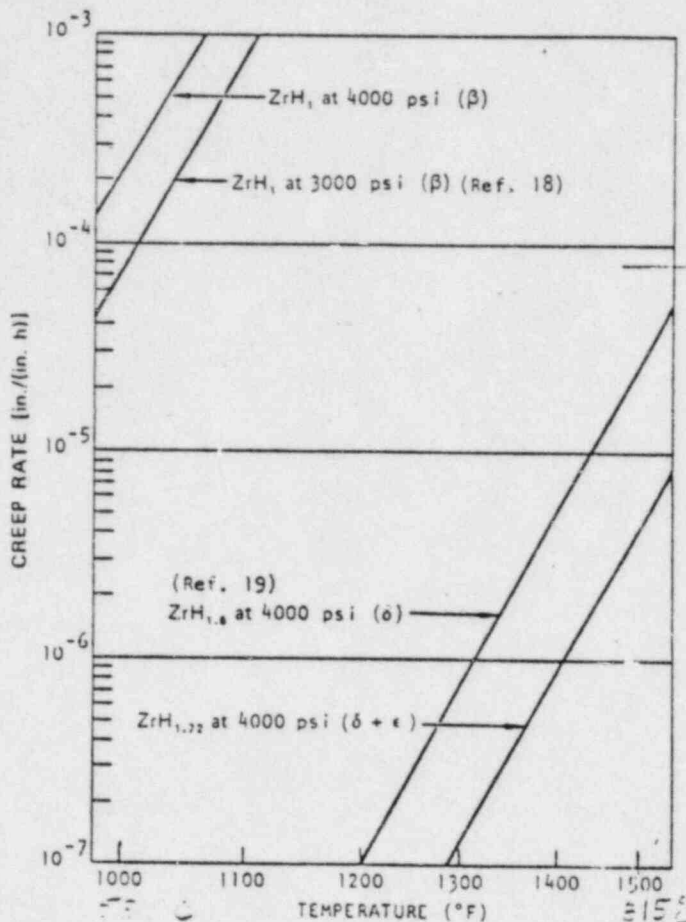


Fig. 5. Creep properties of zirconium hydride; comparison of β with δ and $\delta + \epsilon$ phase material (from Ref. 17).

>1150°C without exceeding the safe level of the internal hydrogen pressure. Test elements with hot spots of ~1175°C have exhibited local swelling after ~200 to 400 pulses. The swelling resulted from internal porosity formed by the gradual nucleation, growth, and migration of hydrogen bubbles toward the surface in the hot-spot region.

In standard nongapped TRIGA fuel, the steady-state power temperature levels increase after pulsing, and the increases are attributed to the formation of a gas gap between the fuel and cladding. The gap formation is caused by the rapid fuel expansion during the pulse heating of the fuel.

An in-pile high-temperature King furnace²² provides a means to investigate the behavior of reactor fuels in high-temperature transients [e.g., high-temperature gas-cooled reactor (HTGR) coated fuel particles] under transient heating conditions by neutron pulsing to over 3000°C.

A rhodium self-powered neutron flux detector has been installed in the Sandia Corporation TRIGA-ACPR fuel elements to determine the

neutron fluxes under pulsing and steady-state power conditions.²³

Much information on irradiation effects on hydride fuels has been generated in the SN reactor program.²¹ The swelling of the UO₂ hydride fuels at high burnups is governed by the basic mechanisms:

1. The accommodation of solid fission products resulting from fission of ²³⁵U. This led to an early estimated growth of ~1.2% to 2% $\Delta V/V$ per metal at 1% burnup. This mechanism is relatively temperature insensitive.
2. The agglomeration of fission gases at elevated temperatures (above 1300°F). This takes place by diffusion of the xenon and krypton to form gas bubbles.
3. A saturable cavity nucleation phenomenon that results from the nucleation and growth of irradiation-formed vacancies into voids over a certain range of temperatures where the voids are stable. The saturation of growth by this mechanism was termed offset swelling. It was deduced from the rapid decrease in fuel-to-cladding ΔT experienced during the early part of the irradiation. This saturation was reached in ~1500 h.

The highest swelling occurs in the beta phase at elevated temperatures by means of the fission gas agglomeration, because the low creep strength of the beta phase cannot accommodate the fission gas pressures in the gas bubbles. Sweeping of fission gases can occur by phase boundary motion if the beta phase forms in the irradiated metal. For example, beta-phase fuel specimens (H/Zr = 1.2 and 1.4) were postirradiation-annealed after low-temperature (700°F) irradiation. Annealing for 211 h at 1300°F produced small amounts of shrinkage, whereas annealing for 75 h at 1600°F produced 15 to 25% swelling. Annealing of delta-phase fuel (H/Zr = 1.6 to 1.9) under the same conditions produced small amounts of shrinkage or swelling—<1.5% in all cases.²⁴ The shrinkage of the fuel on postirradiation annealing is ascribed to recovery of the matrix from damage caused at temperatures lower than those employed during annealing. Anomalous shrinkage can also be attributed to hydrogen loss.

The samples exhibiting large decreases in density showed cracks and voids that suggest fission gas agglomeration.²⁴ The void clusters in high-hydride samples were correlated with epsilon-phase banding, which led to the conclusion that some damage mechanism (other than fission gas agglomeration) takes place based on an apparent delta-epsilon-phase boundary damage phenomenon.²⁵ The epsilon-phase irradiation data indicate

the presence of stress-related fuel growth phenomena since the growth is highly radial oriented.²⁶ An important consideration in integrating the irradiation results is the presence of thermal gradients in the fuel samples in the tests. It is evident from the data that the fuel in the reactor tests and in capsule tests, which closely simulated the SNAP reactor operating conditions and thermal gradients, showed a different temperature-swelling relationship compared to the other capsule fuel experiments where the fuel temperatures were more uniform.²⁶

A number of attempts have been made to correlate the measured swelling of SNAP-reactor fuels with burnup, temperature, and hydrogen content. Large uncertainties in each of these parameters have made it necessary to use a statistical sample²⁷ based on clusters of points rather than on individual datum points.

The observed dimensional changes indicate variations in the ratio of volumetric change to diameter change large enough to establish the importance of diameter as an engineering variable.²⁷ The fuel growth is isotropic only when the ratio of $\Delta V/V$ to $\Delta D/D$ is 3. The fuel growth is preferred in the radial direction when the ratio is between 2 and 3 and in the axial direction when the ratio is greater than 3. Axial shrinkage gives rise to a ratio of <2 . The mean value of the ratio for the delta phase is 2.8 ± 0.4 and for the epsilon phase is $\sim 1.6 \pm 0.5$.

In the swelling correlations used for the SNAP-reactor fuels, the bulk average temperature is considered to be identical to the arithmetic average of the peak centerline temperature and the surface temperature.²⁶ The time-variation of this temperature was calculated from the beginning-of-life (BOL) temperature, based on the cladding thermocouple readings, the measured end-of-life (EOL) fuel $\Delta D/D$, and the following fuel swelling model²⁶:

$$\Delta V/V = 3B + \exp(-K/T) ,$$

where

B = burnup, metal at.%

K = constant ($\sim 30\,000$)

T = bulk average fuel temperature (in degrees R).

As stated earlier, the greatest success has been achieved by using the offset, or equilibrium, bulk average fuel temperature that the fuel reaches after offset swelling has been completed.²⁸ The fuel swelling observed in the SNAP reactor and S8DR reactor experiments was generally lower than predicted from the capsule

tests, except for the NAA-121 capsule test in which the temperature profiles were similar, and the data correlate well with the S8DR data.²⁶

A model of swelling based on burnup and temperature led to the relationship for volumetric growth of²⁸

$$\% \frac{\Delta V}{V} = \alpha B + \beta \exp(-\lambda/T) ,$$

where

$$\% \frac{\Delta V}{V} = 2 \frac{\Delta D}{D} + \frac{\Delta L}{L}$$

B = burnup, metal at.%

T = bulk average fuel temperature

α , β , and λ = constants.

The fuel swelling data are usually plotted as log (corrected volume growth) versus $1/T$. The corrected volume growth for offset swelling has been given variously by

$$\frac{\Delta V}{V} - 2.76B$$

$$\frac{\Delta V}{V} - 2.8B$$

and more recently

$$\frac{\Delta V}{V} - 3B$$

(the Bonzer-Swenson correlation).

The S8DR data tend to show a greater temperature sensitivity than that shown by the Bonzer-Swenson correlation, but the bulk of the data does fall within the Bonzer-Swenson scatter band and indicates a higher temperature sensitivity only if considered as a separate data set.²¹ The S8DR data show a correlation of $[(\Delta V/V) - 3B]$ of 0.8% at 1250°F, 0.4% at 1200°F, and 0.2% at 1150°F. The total correlation is as follows:

$$\left(\frac{\Delta V}{V} - 3b \right) = 5.5 \exp \left(- \left\{ 2.3b \exp \left[\frac{21.5}{2} \left(\frac{1860}{T} - 1 \right) \right] + 21.5 \left(\frac{1860}{T} - 1 \right) \right\} \right) ,$$

where

b = burnup, metal at.%

\dot{b} = burnup rate, burnup/10 000-h operation

T = absolute operating temperature of fuel at the time offset fuel growth has been completed, °R.

PULSE HEATING

The U-ZrH fuel elements used in the TRIGA reactor are capable of operation under conditions of transient experiments for delivery of high-intensity bursts of neutrons. For these experiments, the reactor is equipped with a special control rod mechanism that provides a method of obtaining a step reactivity change of predetermined magnitude in the reactor. During the nuclear pulse, nearly all the energy is stored as thermal energy in the fuel material. This results in an almost instantaneous rise in the temperature of the fuel body. These fuel elements have operated repeatedly in the Advanced TRIGA Prototype Reactor (ATPR) to peak power levels of over 8000 MW, providing a neutron fluence per pulse of $\sim 10^{15}$ n/cm².

The ATPR fuel elements have been subjected to thousands of pulses of 2000 MW and more. The nuclear safety stems from the large prompt negative temperature coefficient of reactivity of the uranium-zirconium hydride fuel-moderator material. The inherent prompt shutdown mechanism of TRIGA reactors has been demonstrated extensively during the tens of thousands of pulses conducted on TRIGA reactors. These tests involved step insertions of reactivity of up to $3\frac{1}{2}\%$ $\delta k/k$. An in-pile test has been performed on fuel elements of a modified design (gapped) for high performance in the TRIGA ACP. As expected, there was satisfactory fuel body performance after 400 pulses at temperatures up to the design point of 1000°C (Ref. 2). There was no evidence of interaction between the clad and the fuel. The transient gas pressure in the space between the fuel and the clad was measured during the pulse, and peak pressures were found to be <40 psia—well below the upper bound implied by the equilibrium pressure data. As testing at higher temperatures continued, there was some evidence that at hot-spot regions, where the temperature near the fuel surface reached $\sim 1200^\circ\text{C}$, the fuel gradually swelled slightly over a large number of pulses under the influence of the hydrogen pressure in small bubbles that nucleated in the hot spots ($\sim 1200^\circ\text{C}$ U-ZrH₂) and formed a gray patch.

The basic cause of fuel-body distress after more than 200 pulses appears to be local overheating of the fuel body as a result of thermal-neutron flux peaking in a water-cooling channel adjacent to the fully enriched special test elements. The mechanism by which this apparently occurs is as follows: If the internal temperature and hydrogen concentrations are sufficient to produce bubbles of hydrogen gas, plastic or creep yielding of the fuel material over successive

pulses can permit an increase in bubble size over a period of time; this would tend to increase the disruptive force while weakening the restraining matrix, thus gradually producing the porous expanded fuel that constitutes the gray patch. Other possible mechanisms were considered but have been tentatively rejected on the basis of evidence obtained in the postexperiment analyses.

There was no indication that excessive heat transfer rates contributed to cladding distortion. Cladding material cut from the fuel element appeared to be straight and true. When the initial longitudinal cut was made, the cladding sprang open slightly to a uniform gap of $\sim \frac{3}{16}$ in., as would be expected from the residual stress remaining due to the action of the die in the final drawing operation during fabrication. The thermal-stress distortion due to excess heat flux would have tended to produce residual stress in the opposite direction. Additionally, the external surface discoloration was far less than for a normal element after either normal maximum steady-state or pulsed operation.

Internal gas pressure was indicated to be negligible compared with the yield point for the cladding. The pressure-transducer calibration was rechecked and found satisfactory after the test. In addition, a pressure-instrumented element (identified as 2E) was pressurized to 50 psi after the test and maintained pressure overnight, which verified that there was no measurable leakage in the element. Note that gas chromatography was performed for gas extracted from element 2E. In this test no hydrogen gas was detected, although the instrument has a high sensitivity for hydrogen.

The mechanisms of nucleation, growth, and migration of gas bubbles in solids have been studied extensively in recent years, mainly in connection with fission gas formation and swelling in nuclear reactor fuels and helium formation by nuclear transmutation in alloys. This information is most useful in elucidating the damage mechanism. Barnes and Nelson,²⁹ Nichols,³⁰ and Lawton et al.³¹ have presented reviews of this subject. The conclusions reached from the results of recent studies can be summarized as follows: The behavior of the gas bubbles determines how much gas is released and how much swelling is produced by the gas retained. The bubbles can migrate bodily under the influence of various driving forces and by various mechanisms. In most cases, the bubbles migrate in a direction determined by temperature gradients, stress gradients, and moving dislocation lines or grain boundaries. Analysis indicates that small bubbles are dominated by the behavior of dislocation lines; however, as their size increases the temperature gradient becomes more important. Models based

on the behavior of dislocation lines and grain boundaries are appropriate when the temperature gradients are small; little gas escapes and swelling is then the main consequence.

On the other hand, it has been found that where steep temperature gradients occur the behavior is more complex, different models being required for each temperature zone. At high temperatures the gradient can drag bubbles from dislocations and the bubbles migrate up the temperature gradients, becoming trapped on grain boundaries where gas is released periodically. Barnes and Nelson²⁹ have postulated that bubbles migrate predominantly by a surface diffusion mechanism, while the main parameters determining the behavior of the bubbles are temperature gradient, bubble radius, surface diffusion, vapor pressure of the solid, and surface tension. If the material is stressed, the moving dislocations will drag the bubbles. The temperature gradient largely determines the critical sizes of the bubbles.

These observations are in line with the conclusions of the present authors regarding the mechanism of formation of the distressed area in the hot spots in the pulsed special test fuel. This area evidently was subjected to a temperature range and to cycles of thermal gradients and stress gradients such as to favor the nucleation, migration, and growth of hydrogen bubbles toward the surface.

As the bubbles grew in size, the internal hydrogen pressure could not be accommodated by the fuel matrix, which consequently gradually yielded and swelled until it made contact with the cladding. With subsequent pulses, the cladding itself is compelled to make provision for fuel swelling by its own expansion. That is, the cladding will be deformed by swelling of the fuel body at temperatures below those where creep or hydrogen gas pressure can cause the cladding to expand. This phenomenon follows from the fact that in a pulse, the rapidly heated fuel expands thermally more than the cladding and thereby forces the cladding to expand once the initial gap has been bridged by swelling of the fuel body. This accounts rather well for the fact that the changes are governed by a progressive process and do not take place during a few pulses.

From the results of these tests it can be concluded that U-ZrH fuel elements can be safely pulsed even to very high fuel temperatures. Not until after more than 400 pulses to fuel temperatures in excess of 1100°C did measurements show that two of the five test elements exceeded the conservative dimensional tolerances. In the first 200 pulses, there was no external evidence of change in any of the five special test elements. The practical consequences of this are several-

fold. First, small power reactors using U-ZrH fuel can safely sustain accidental power excursions to high fuel temperature. Second, and perhaps more significant, high-level pulsing reactors (fluences of 10^{15} n/cm²) can be operated with U-ZrH fuel with a reasonable fuel lifetime. Furthermore, with regard to standard types of TRIGA research reactors, it is evident that present peak fuel temperatures are conservative.

The results of rapid dehydriding tests indicate that the endothermic nature of hydrogen loss slows down the rate of temperature rise. When the hydride specimens are rapidly heated to elevated temperatures in a dynamic vacuum system, large-scale internal cracking takes place, whereas when a backpressure of hydrogen is maintained (as in a clad fuel element), the hydride fuel body contains relatively small bubbles that are associated with the grain structure and substructure of the material.

In the SNAPTRAN (Ref. 32), TREAT (Ref. 33), and KIWI-TNT (Ref. 34) tests, high-hydride modified U-Zr hydrides have been pulse-heated to destruction. In these specimens the hydrogen content (1.82 wt%) was very high, and the temperatures were high enough to rupture or granulate the fuel. In the KIWI-TNT transient tests, the specimens were exposed to a large nuclear transient under the following conditions³⁴:

1. in containers designed to withstand internal pressures of 120 000 psi
2. in chambers sealed with prestressed rupture disks calibrated to burst within 5% of specific pressure values
3. in a container in which the hydrogen gas released from the fuel acted on a free piston that impacted a copper anvil and produced an indentation calibrated to give a measure of the gas pressure as a function of time.

In type 3 tests, extensive cracking of the fuel took place since large void space was available. Much less fracture occurred in type 1 and type 2 tests since the internal gas pressure was balanced. In some of the TREAT tests, the fuel irradiation temperatures were high enough to melt the fuel (~1800°C).

Measurements and calculations have been reported of hydrogen loss from hydrided 10 wt% U-Zr fuel elements (1.25 in. diam × 1.0 in. long) that were rapidly heated by induction to temperatures near the melting point.³⁵ Results indicated that within ~75 sec, the surface temperature in a nonoxidizing atmosphere reached 1700 to 1780°F with only minor hydrogen evolution. Abruptly thereafter, the surface was observed to crack.

parallel with the cylindrical axis, with strong outgassing rates, and the temperature dropped. After a few seconds the temperature again began to rise and outgassing continued. After ~3 min at surface temperatures of 2010 to 2020°F, the specimen was cooled. Subsequent analysis showed large amounts of residual hydrogen. In another series, temperatures up to 3400°F were reached before power was shut off. In these, almost all the hydrogen was driven off. The volume of the sample was found to have decreased, and the surface cracks visibly healed as the temperature rose above 2000°F.

FISSION-PRODUCT RETENTION

A number of experiments have been performed to determine the extent to which fission products are retained by U-ZrH fuel. These experiments were conducted over a period of 11 yr and under a variety of conditions. Results prove that only a small fraction of the fission products are released, even in completely unclad U-ZrH fuel. The release fraction varies from 1.5×10^{-5} for an irradiation temperature of 350°C to $\sim 10^{-2}$ at 800°C (Ref. 36). The experiments on fission-product release include:

1. 1960—the measurement of the quantity of a single fission-product isotope released from a full-size TRIGA element during irradiation.
2. 1966—the measurement of the fractional release of several isotopes from small specimens of TRIGA fuel material during and after irradiation at temperatures ranging from ~25 to 1100°C.
3. 1971—the measurement of the quantities of several fission-product isotopes released from a full-size TRIGA fuel element during irradiation in a duplication of the 1960 experiment.

Postirradiation-annealing measurements of the release from small fuel samples heated to 400°C.

Postirradiation-annealing release measurements from a small previously irradiated fuel sample that had experienced fuel burnup to ~5.5% of the ^{235}U .

4. SNAP—measurements made as part of the Space Nuclear Auxiliary Power reactor program.

The experiments show that there are two mechanisms involved in the release of fission products from TRIGA fuel each of which predominates over a different temperature range. The first mechanism is that of fission fragment

recoil into the gap between the fuel and clad. This effect predominates in fuel at temperatures up to ~400°C; the recoil release rate is dependent on the fuel surface-to-volume ratio but is independent of fuel temperature. Above ~400°C the controlling mechanism for fission-product release from TRIGA fuel is diffusion and the amount released is dependent on the fuel temperature, the fuel surface-to-volume ratio, the time of irradiation, and the isotope half-life.

The results of the TRIGA experiments and measurements by others of fission-product release from SNAP fuel have been compared and found to be in good agreement.

The fractional release, ϕ , of fission-product gases into the gap between fuel and clad from a full-size standard TRIGA fuel element is given by

$$\phi = 1.5 \times 10^{-5} + 3.6 \times 10^3 \exp[-1.34 \times 10^4 / (T + 273)]$$

where T is the fuel temperature °C. This function is plotted in Fig. 6. The first term of this

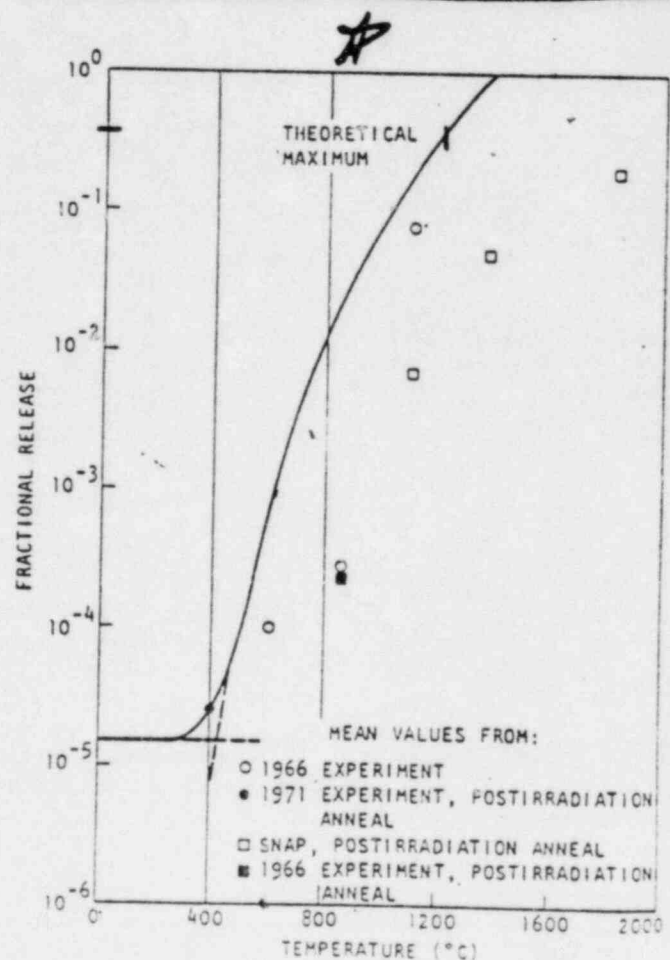


Fig. 6. Fractional release of gaseous fission products from TRIGA fuel showing theoretical maximum, and experimental values above 400°C corrected to infinite irradiation.

function is a constant for low-temperature release; the second term is the high-temperature portion.

The release fractions have all been normalized to a standard full-size fuel element geometry, although individual measurements were made with different geometry.

The curve in Fig. 6 applies to a fuel element that has been irradiated for a time sufficiently long that all fission-product activity is at equilibrium and the release fraction is at its theoretical maximum. Figure 6 shows that the measured values of fractional releases fall well below the curve. Therefore, for safety considerations, this curve gives very conservative values for the high-temperature release from TRIGA fuel.

Also worthy of note are the following conclusions from the TRIGA fission-product release experiments:

1. Because the samples were unclad, the high-temperature measurements were made on essentially dehydrided U-Zr. Postirradiation-annealing measurements indicate that the dehydriding process did not significantly affect the release rate.

2. Part of the 1971 experiments was the measurement of the release from a postirradiation anneal of a sample of fuel that had been irradiated to a burnup of ~5.5% of the ^{235}U (or 1.1% of the total uranium atoms). The results of this part of the experiment indicated that the effects of long-term irradiation of the fuel on fission-product release are small, at least for total burnup equivalent of the maximum that has been achieved.

3. The release fraction for accident conditions is characteristic of the normal operating temperature, not the temperature during accident conditions. This is because the fission products released as a result of a fuel clad failure are those that have collected in the fuel-clad gap during normal operation.

4. Since the fuel temperature distribution is not isothermal it is necessary to integrate the temperature-dependent release fraction over the temperature distribution in a fuel element.

PROMPT NEGATIVE TEMPERATURE COEFFICIENT

The basic parameter that allows the TRIGA reactor system to operate safely during either steady-state or transient conditions is the prompt negative temperature coefficient of reactivity associated with the TRIGA fuel and core design. This temperature coefficient (λ) is the primary determinant of the behavior of a TRIGA core during pulse operation and allows great freedom

in steady-state operation, as the effect of accidental reactivity changes occurring from experimental devices in the core is greatly reduced.

The basic physical processes that occur when the fuel-moderator elements are heated can be described as follows: The rise in temperature of the hydride increases the probability that a thermal neutron in the fuel element will gain energy from an excited state of an oscillating hydrogen atom in the lattice. As the neutrons gain energy from the ZrH, their mean-free-path is increased appreciably. This is shown qualitatively in Fig. 7 for a standard TRIGA cell. Since the average chord length in the fuel element is comparable with a mean-free-path, the probability of escape from the fuel element before capture is increased. In the water (where the temperature remains relatively constant), the neutrons are rapidly rethermalized so that the capture and escape probabilities are relatively insensitive to the energy with which the neutron enters the water. The heating of the moderator mixed with the fuel thus causes the spectrum to harden more in the fuel than in the water. As a result, there is a temperature-dependent disadvantage factor for the unit cell in the core that decreases the ratio of absorptions in the fuel to total-cell absorptions as the fuel element temperature is increased. This change in disadvantage factor brings about a shift in the core neutron balance, giving a loss of reactivity, and is termed the cell effect.

The prompt negative temperature coefficient for the TRIGA-FLIP core is based on the same core spectrum hardening characteristic that occurs in a standard TRIGA core. However, for a TRIGA-FLIP (70% enriched) fuel element, the uranium loading is ~3.5 times that of a standard TRIGA element, and this causes the neutron mean-free-path in the FLIP element to be much shorter. For this reason, the escape probability

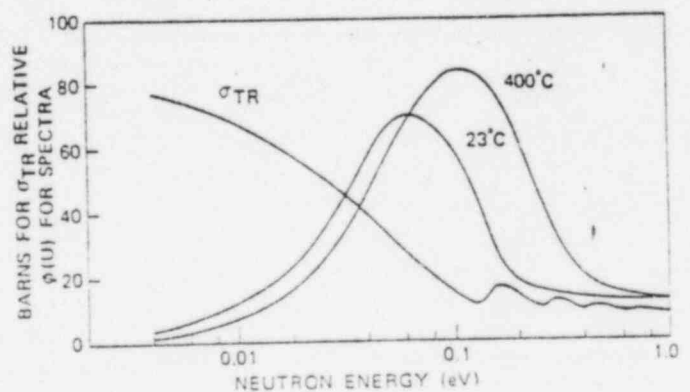


Fig. 7. Transport cross section for hydrogen in zirconium hydride and average spectra in TRIGA $\text{ZrH}_{1.7}$ fuel element for 23 and 400°C fuel.

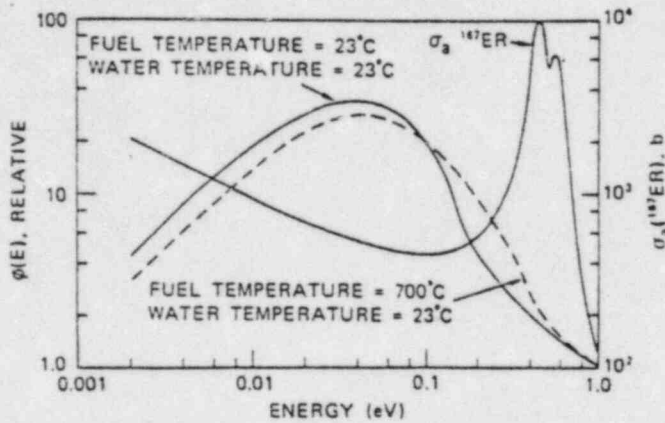


Fig. 8. Thermal-neutron spectra versus fuel temperature relative to σ_a versus energy for ^{167}Er .

for neutrons in the fuel is not greatly enhanced as the fuel-moderator material is heated. In the TRIGA-FLIP fuel, the temperature hardened spectrum is used to decrease reactivity through its interaction with a low energy resonance material. Thus erbium, with its double resonance at ~ 0.5 eV, is used in the TRIGA-FLIP fuel both as a burnable poison and as a material to enhance the prompt negative temperature coefficient. The neutron spectrum shift, pushing more of the thermal neutrons into the ^{167}Er resonance as the fuel temperature increases, is illustrated in Fig. 8, where the cold and hot core spectra are plotted along with the energy-dependent absorption cross section for ^{167}Er . As with a standard TRIGA core, the temperature coefficient is prompt because the fuel is intimately mixed with a large portion of the moderator, and thus fuel and solid moderator temperatures rise simultaneously, producing the temperature-dependent spectrum shift.

For the reasons just discussed, more than 50% of the temperature coefficient for a standard TRIGA core comes from the temperature dependent disadvantage factor, or cell effect, and $\sim 20\%$ each from Doppler broadening of the ^{238}U resonances and temperature-dependent leakage from the core. These effects produce a temperature coefficient of $\sim 9.5 \times 10^{-5}/^\circ\text{C}$, which is rather constant with temperature. On the other hand, for a TRIGA-FLIP core, the effect of cell structure on the temperature coefficient is small. Almost the entire coefficient comes from temperature-dependent changes in ηf within the core, and $\sim 80\%$ of this effect is independent of the cell structure. The calculated BOL temperature coefficient is shown in Fig. 9 for 70% enriched TRIGA-FLIP fuel. It increases rapidly as a function of fuel temperature because of the steadily increasing number of thermal neutrons being pushed into

the ^{167}Er resonance. The temperature-dependent character of the temperature coefficient of TRIGA-FLIP core is advantageous in that a minimum reactivity loss is incurred in reaching normal operating temperatures, but any sizeable increases in the average core temperature result in a sizably increased prompt negative temperature coefficient to act as a shutdown mechanism. Calculations show the temperature coefficient to be insensitive to the change in configuration from a compact core to the operational core with four flux traps containing either water or typical experiments. Burnup calculations indicate that after 3000 Mwd of operation, the ^{235}U concentration averaged over the core is $\sim 67\%$ and the ^{167}Er concentration is $\sim 33\%$ of the BOL values. Temperature coefficient calculations for the burned-up core, including fission products, gave results as shown in Fig. 9. The EOL coefficient is less temperature dependent than the BOL coefficient because of the sizeable loss of ^{167}Er and the resulting increased transparency of the ~ 0.5 -eV resonance region to thermal neutrons.

The temperature coefficient, therefore, depends on spatial variations of the thermal-neutron spectrum over distances of the order of a mean free-path with large changes of mean-free-path occurring because of the energy change in

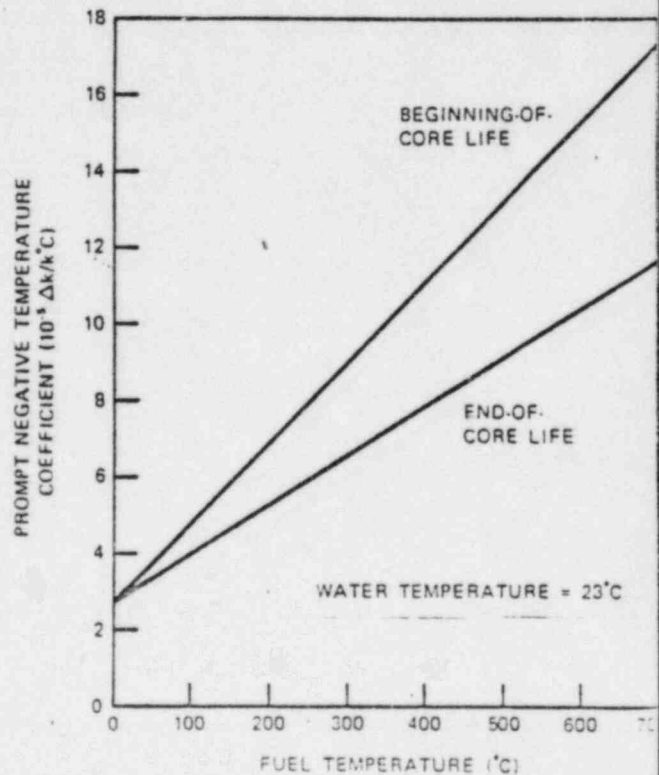


Fig. 9. Calculated prompt negative temperature coefficient versus fuel temperature at BOL and EOL—70% enriched TRIGA-FLIP fuel.

single collision. A quantitative description of these processes requires a knowledge of the differential slow-neutron energy transfer cross section in water and zirconium hydride, the energy dependence of the transport cross section of hydrogen as bound in water and zirconium hydride, the energy dependence of the capture and fission cross sections of all relevant materials, and a multigroup transport theory reactor description that allows for the coupling of groups by speeding up as well as by slowing down.

Qualitatively, the scattering of slow neutrons by zirconium hydride can be described by a model in which the hydrogen atom motion is treated as an isotropic harmonic oscillator with energy transfer quantized in multiples of ~ 0.14 eV. More precisely, the SUMMIT (Ref. 37) model uses a frequency spectrum with two branches, one for the optical modes for energy transfer with the bound proton, and the other for the acoustical modes for energy transfer with the lattice as a whole. The optical modes are represented as a broad frequency band centered at 0.14 eV, and with the width adjusted to fit the cross-section data of Woods et al.³⁸ The low-frequency acoustical modes are assumed to have a Debye spectrum with a cutoff of 0.02 eV and a weight determined by an effective mass of 360.

This structure allows a neutron to thermalize by losing energy in units of ~ 0.14 eV as long as its energy is above 0.14 eV. Below 0.14 eV the neutron can still lose energy by the inefficient process of exciting acoustic Debye-type modes in which the hydrogen atoms move in phase with the zirconium atoms, which in turn move in phase with one another. These modes therefore correspond to the motion of a group of atoms having a mass much greater than that of hydrogen, and indeed even greater than the mass of zirconium. Because of the large effective mass, these modes are very inefficient for thermalizing neutrons, but for neutron energies below 0.14 eV, they provide the only mechanism for neutron slowing down. (In a TRIGA core, the water provides for ample neutron thermalization below 0.14 eV.) In addition, it is possible for a neutron in the ZrH to gain one or more energy units of ~ 0.14 eV in one or several scatterings from excited Einstein oscillators. Since the number of excited oscillators present in a ZrH lattice increases with temperature, this process of neutron speeding up is strongly temperature dependent and plays an important role in the behavior of ZrH-moderated reactors.

For calculations of the prompt negative temperature coefficient, all neutron cross sections for energies above thermal (>1.125 eV) were generated using the GGC-5 code,³⁹ where fine-

group (~ 100 groups) cross sections, stored on tape for all commonly used isotopes, are averaged over a spatially independent flux derived by solution of the *B-1* equations for each discrete reactor region composition. This code and its related cross-section library predict the age of each of the common moderating materials to within a few percent of the experimentally determined values. The resonance integral method of Adler et al.⁴⁰ is used to generate cross sections for resonance materials.

The core thermal cross sections were generated using the multigroup cross-section GTF code.⁴¹ GTF computes the spatially dependent thermal spectra at each mesh point in the cell, using the discrete ordinates method and the fine-group (58-point) cross-section data contained in the thermal portion of the GGC-5 code.

Cell-averaged broad-group cross sections are those obtained by averaging the 58-point cross sections over the space-dependent spectrum. In the past, cell-averaged thermal-group cross sections have been generated by first obtaining broad-group cross sections averaged over a 58-point spectrum for the homogenized (space-independent) cell. Using these cross sections, a separate cell calculation was then done to obtain broad-group disadvantage factors for each of the regions in the cell. The broad-group disadvantage factors were then used in a space-independent spectrum calculation to generate the cell-averaged cross sections. The use of 58 thermal-group cross sections in the GTF code versus the broad-group cell method just described, results in a more accurate ratio of $\sigma_a(^{167}\text{Er})/\sigma_a(^{235}\text{U})$ for the cell-averaged broad groups in the erbium resonance range. This ratio can affect the calculated reactivity of the core, but more important is its effect on the calculated life of the core. Cross sections calculated with GTF have a smaller ratio of $\sigma_a(^{167}\text{Er})/\sigma_a(^{235}\text{U})$ than those from a standard broad-group cell calculation. This smaller ratio gives a shorter calculated lifetime for the core.

Scattering kernels were used to describe the interactions of the neutrons with the chemically bound moderator atoms. The bound hydrogen kernels for hydrogen in water were generated by the THERMIDOR code,⁴² while those for hydrogen in zirconium hydride were generated by SUMMIT. These scattering models have been used to adequately predict the water and hydride (temperature-dependent) spectra as measured at the General Atomic linear accelerator.^{43,44} Figure 10 illustrates the agreement between calculations and experiments for thermal-neutron spectra in ZrH.

TRIGA temperature coefficients have been determined numerically⁴⁵ by calculating the change in reactivity associated with a uniform heating of

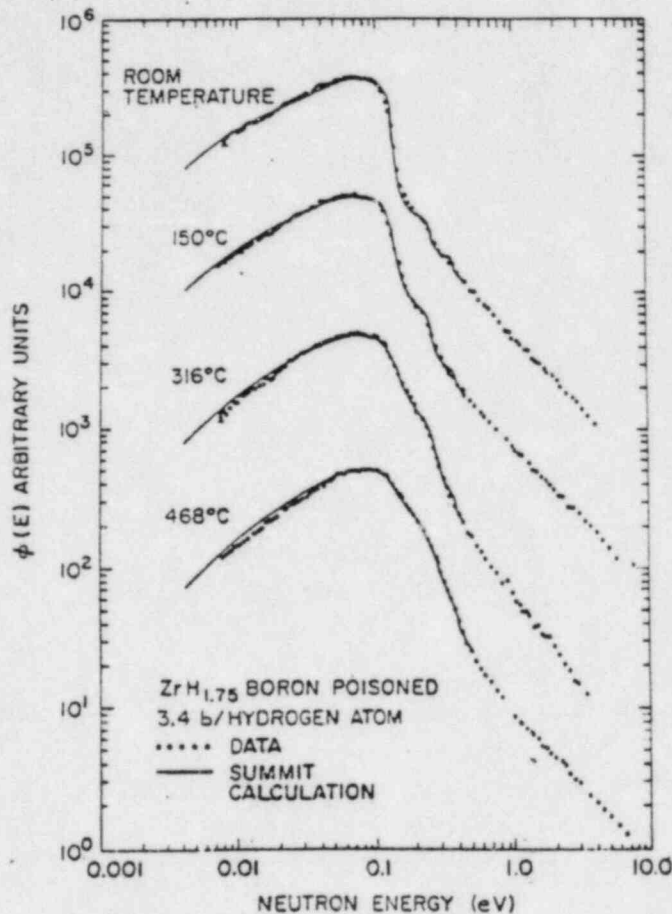


Fig. 10. Experimental and theoretical neutron spectra from ZrH_{1.75} showing the effect of temperature variation calculations done with the SUMMIT code.

the fuel-moderator elements, with the core water and reflector materials assumed to remain at 23°C. The effects of variation from a uniform temperature for both the cell and the entire core have been investigated with the results that the cell effect is reduced by ~5%, but this decrease is offset by an increase in the core leakage contribution to the prompt negative temperature coefficient of ~10% when zones of different temperature are incorporated in the cell and reactor calculations.

OTHER PULSING PARAMETERS

To perform kinetics calculations and interpret experiments, calculations have been made to determine both the effective delayed neutron fraction, β_{eff} , and the prompt neutron lifetime, l . Calculated values are shown in Table I, where it is seen that the effective delayed neutron fraction was found to be insensitive to the reflector material.

The β_{eff} values were derived from reactor calculations where the reactivity was first computed with the prompt fission spectrum alone and then recalculated with the fission spectrum accounting for both prompt and delayed neutrons. The two k_{eff} values thus obtained, k_p and k_l , were used in the relationship

$$\beta_{eff} = \frac{k_l(1 + \beta_0)}{k_p} - 1$$

where β_0 is the actual delayed neutron fraction (0.0065).

The prompt neutron lifetime was calculated by the $1/v$ absorber method, where a very small amount of boron is homogeneously distributed throughout the system, and the resulting change in reactivity is related to the neutron lifetime as follows:

$$l = \frac{\delta k_{eff}}{\omega}$$

where ω is related to the boron atomic density

$$N_{boron} = \frac{\omega}{\sigma_0 v_0} = 6.024 \times 10^{-9} (\times 10^{24}) \times \omega$$

and

N_B = boron atomic density

ω = integer

v_0 = 220 000 cm/sec

σ_0 = 755 b (2200 m/sec boron cross section)

and δk_{eff} is the difference in reactivity between a pair of calculations—one in which the system contains no boron and one in which it does. The calculation was found to be insensitive to change in ω between 1 and 100.

The enthalpy of TRIGA fuel material as a function of temperature has been determined from data and fundamental considerations given in a paper by Douglas,⁴⁶ which lends itself fairly readily to different conditions of composition.

Douglas measured the heat content of different samples of zirconium hydride ranging, in hydrogen atomic percent, from 0 to 55.5 (Refs. 46, 47 and 48). From the results of these measurements, and knowing the compositions at the phase boundaries, one can extrapolate the Douglas data to values of x (hydrogen atoms per zirconium atom) > 1.25 (the highest value of the sample studied). This was done to derive an expression for the heat content (above 25°C) of the δ phase ZrH_x, which is approximated well by the following relationship:

TABLE I
Calculated Values of l and β_{eff}

Fuel Element	Reflector	
	Water	Graphite
Stainless-steel clad 8 wt% U-ZrH _{1.7} (20% enriched U)	$\beta_{eff} = 0.0070$ $l = 39 \times 10^{-6}$ sec	$\beta_{eff} = 0.0070$ $l = 43 \times 10^{-6}$ sec
Aluminum clad 8 wt% U-ZrH _{1.0} (20% enriched U)	$\beta_{eff} = 0.0073$ $l = 45 \times 10^{-6}$ sec	$\beta_{eff} = 0.0073$ $l = 60 \times 10^{-6}$ sec
Stainless-steel clad 12 wt% U-ZrH _{1.6} (20% enriched U) ACPR	$\beta_{eff} = 0.0073$ $l = 32 \times 10^{-6}$ sec	
Stainless-steel clad 1.6 wt% Er, 8.5 wt% U-ZrH _{1.6} (70% enriched U) TRIGA-FLIP	$\beta_{eff} = 0.0071$ $l = 16 \times 10^{-6}$ sec (BOL) 20×10^{-6} sec (EOL)	

$$(H - H_{25})_{ZrH_x} = 0.03488 T^2 + [34.446 + 14.8071(x - 1.65)] T - 882.95 - 370.18(x - 1.65) \text{ J/mole}$$

The temperature is in °C.

The enthalpy of uranium metal was derived from the specific heat data given by Etherington⁴⁹ and is given by

$$(H - H_{25})_U = (0.6525 \times 10^{-4} T^2 + 0.1094 T - 2.776) \text{ J/g}$$

Using the expression

$$\rho_{ZrH_x} = 6.49 - 0.55 H/Zr \text{ g/cm}^3$$

where $H/Zr < 1.6$ and $\rho_{ZrH_x} < 1.6 = \rho_{ZrH_{1.6}}$, a density of 5.610 g/cm^3 was computed for the $ZrH_{1.6}$. Using 18.9 g/cm^3 for the uranium metal density, the density of the 8.5 wt% U-ZrH_{1.6} is 5.9768 g/cm^3 . Then the volumetric heat content of 8.5 wt% U-ZrH_{1.6} alloy is calculated to be

$$H - H_{25}(8.5U - ZrH_{1.6}) = 2.08 \times 10^{-3} T^2 + 2.04 T - 52.2 \text{ W-sec/cm}^3$$

and the volumetric specific heat is

$$C_p = 2.04 + 4.17 \times 10^{-3} T \text{ W-sec/cm}^3 \text{ } ^\circ\text{C}$$

(from 0°C)

For the 385-cm³ (15-in.-high) fueled portion of a TRIGA fuel element, one obtains

$$C_p = 825 + 1.61(T - 25^\circ\text{C}) \text{ W-sec/}^\circ\text{C element}$$

(from 25°C)

LIMITING DESIGN BASIS PARAMETER AND VALUES

Fuel-moderator temperature is the basic limit of TRIGA reactor operation. This limit stems from the out-passing of hydrogen from the ZrH_{1.6} and the subsequent stress produced in the fuel element clad material. The strength of the clad as a function of temperature can set the upper limit on the fuel temperature. A fuel temperature safety limit of 1150°C for pulsing stainless-steel U-ZrH_{1.65} or Er-U-ZrH_{1.65} fuel is used as a design value to preclude the loss of clad integrity when the clad temperature is below 500°C. When clad temperatures can equal the fuel temperature the fuel temperature limit is 950°C. There is also a steady-state operational fuel temperature design limit of 750°C based on consideration of irradiation- and fission-product-induced fuel growth and deformation. This is a time- and temperature-dependent fuel growth as discussed earlier. A maximum temperature of 750°C has been used as the operational design basis temperature because resulting average fuel temperatures result in

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insignificant calculated fuel growth from temperature-dependent irradiation effects. (For ACPR fuel, where burnup is extremely low, the steady-state operational fuel temperature design criterion is 500°C.)

The dissociation pressure of the zirconium-hydrogen system is the principal contributor to the fuel element internal pressure at fuel temperatures above ~800°C. Below ~800°C trapped air and fission-product gases can be the major contributors to the internal pressure. At equilibrium condition this pressure is a strong function of not only temperature but also the ratio of hydrogen to zirconium atoms and the carbon content of the material. The current upper limit for the hydrogen-to-zirconium ratio is 1.65; the design value is 1.6. The carbon content is currently ~0.2% (2000 ppm). The equilibrium hydrogen pressure as a function of temperature for the fuel is shown in Fig. 11. Figure 12 shows the temperature-dependent strength curves currently used for stainless steel in TRIGA design work.

For the ACPR fuel, optimized for pulsing with a built-in thermal barrier (0.01-in. gap) between the fuel and clad, the clad temperature does not

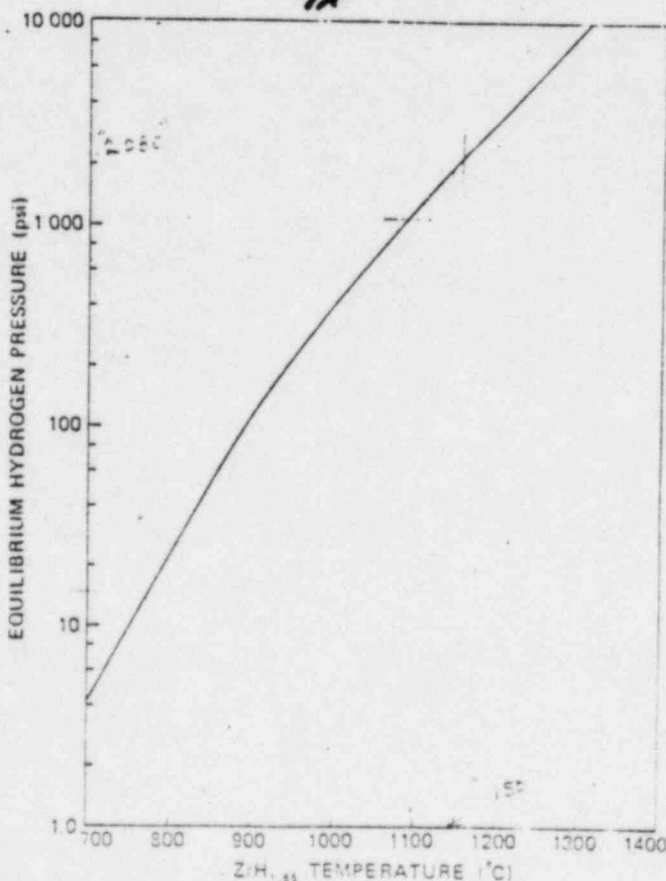


Fig. 11. Equilibrium hydrogen pressure over $ZrH_{1.65}$ versus temperature.

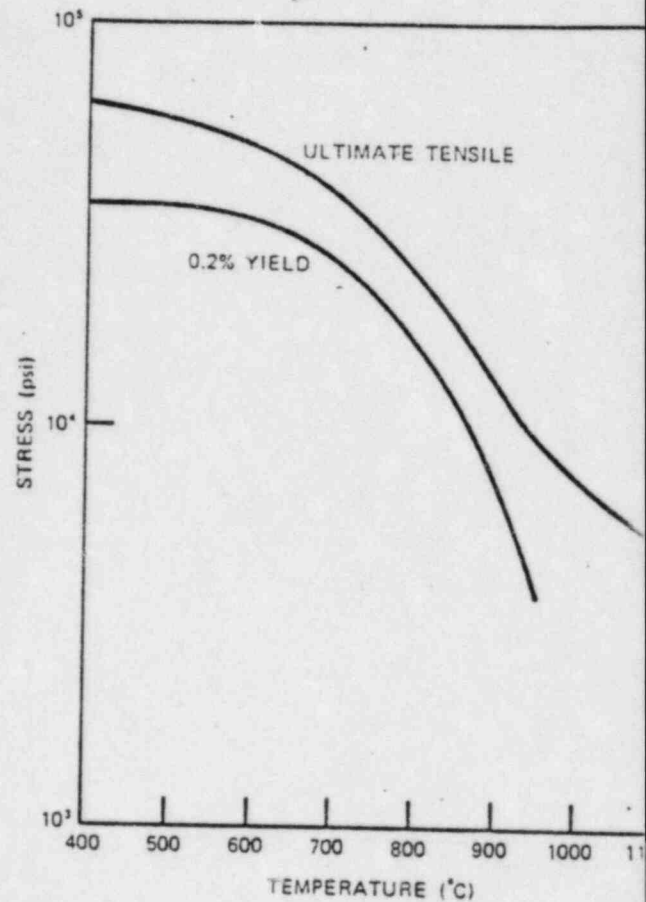


Fig. 12. Strength of Type 304 stainless steel as a function of temperature.

exceed 280°F (138°C). At 250°F (138°C) the yield strength of Type 304L stainless steel is 38 000 psi and the ultimate strength is 68 000 psi (Ref. 50). The stress imposed on the clad S by the internal pressure is

$$S = \frac{r_c}{t_c} P_h$$

where

r_c = clad radius

t_c = clad thickness (in the same units as r_c)

P_h = hydrogen pressure.

For the dimensions of the clad, the maximum allowable hydrogen pressure is

$$P_h = \frac{38\,000}{0.739/0.020} = 1025 \text{ psi}$$

to produce yield, and

$$P_h = \frac{68\,000}{0.739/0.020} = 1840 \text{ psi}$$

to rupture the clad. From Fig. 11 it can be seen that for U- $ZrH_{1.65}$ the fuel temperatures that

produce these pressures, under equilibrium conditions, are 1080 and 1140°C

The equilibrium condition defined above never occurs, however, because the fuel is not at a constant temperature over the whole volume. Consequently, the hydrogen pressures will be much lower than the equilibrium values calculated for the maximum temperature. As hydrogen is released from the hot fuel regions, it is taken up in the cooler regions, and the equilibrium that is obtained is characteristic of some temperature lower than the maximum. To evaluate this reduced pressure, diffusion theory is used to calculate the rate at which hydrogen is evolved and reabsorbed at the fuel surface.

Ordinary diffusion theory⁵¹ provides an expression for describing the time-dependent loss of gas from a cylinder:

$$\frac{\bar{c} - c_f}{c_i - c_f} = \sum_{n=1}^{\infty} \frac{4}{\xi_n^2} \exp\left(-\frac{\xi_n^2 Dt}{r_0^2}\right), \quad (4)$$

where

\bar{c} , c_i , c_f = average, initial, and final gas concentration in the cylinder, respectively

ξ_n = roots of the equation $J_0(x) = 0$

D = diffusion coefficient for the gas in the cylinder

t = time

r_0 = radius of the cylinder.

Setting the term on the right side of Eq. (4) equal to κ , one can rewrite Eq. (4) as

$$\bar{c}/c_i = c_f/c_i + (1 - c_f/c_i)\kappa, \quad (5)$$

and the derivative in time is given by

$$\frac{d(\bar{c}/c_i)}{dt} = (1 - c_f/c_i) \frac{d\kappa}{dt}. \quad (6)$$

Equation (6) represents the fractional release rate of hydrogen from the cylinder, $f(t)$. The derivative of the series in the right side of Eq. (6) was approximated by

$$\frac{d\kappa}{dt} = -[7.339 \exp(-8.34\epsilon) + 29.88 \exp(-249\epsilon)] d\epsilon/dt, \quad (7)$$

where $\epsilon = Dt/r_0^2$.

The diffusion coefficient for hydrogen in zirconium hydride⁵² in which the H/Zr ratio is between 1.56 and 1.86, is given by

$$D = 0.25 \exp[-17800/R(T - 273)], \quad (8)$$

where R is the gas constant and T is the zirconium hydride temperature in °C

Equation (6) describes the escape of gas from a cylinder through diffusion until some final concentration is achieved. Actually, in the closed system considered here, not only does the hydrogen diffuse into the fuel/fuel-clad gap, but it also diffuses back into the fuel in the regions of lower fuel temperature. When the diffusion rates are equal, an equilibrium condition will exist. To account for this, Eq. (6) was modified by substituting for the concentration ratios the ratio of the hydrogen pressure in the gap P_h to the equilibrium hydrogen pressure P_e . Thus, Eq. (6) is rewritten as

$$f(t) = \frac{d(\bar{c}/c_i)}{dt} = [1 - P_h(t)/P_e] \frac{d\kappa}{dt}, \quad (9)$$

where the hydrogen pressure $P_h(t)$ is now a function of time and P_e is the equilibrium hydrogen pressure over the zirconium hydride which is a function of the fuel temperature.

The rate of change of the internal hydrogen pressure, in psi, inside the fuel element cladding is

$$\frac{dP_h}{dt} = \frac{14.7 f(t) \eta_h}{6.02 \times 10^{23}} \left(\frac{22.4}{V_g} \right) \left(\frac{T + 273}{273} \right), \quad (10)$$

where

η_h = number of molecules ⁰⁺ in H₂ in the fuel

T = gas temperature, °C

$f(t)$ = fractional loss rate from Eq. (9)

V_g = free volume inside the fuel clad in liters.

As the atom density of hydrogen in ZrH_{1.66} is $\sim 5.60 \times 10^{22}$ atoms/cm³ and the fuel volume is 366 cm³, η_h is $\sim 1.02 \times 10^{25}$ molecules (H₂). The gap volume is assumed to consist of a 10-mil annulus 15 in. long plus a cylindrical volume, at the top of the element, $\frac{1}{8}$ in. high with a diameter of 1.438 in., for a total of 14.36 cm³. Also, the temperature of the hydrogen in the gap was assumed to be the temperature of the clad. The effect of changing these two assumptions was tested by calculations in which the gap volume was decreased by 90%, and the temperature of the hydrogen in the gap was set equal to the maximum fuel temperature. Neither of these changes resulted in maximum pressures different from those based on the original assumptions although the initial rate of pressure increase was greater. For these conditions

$$P_h = 1.406 \times 10^3 (T + 273) \int f(t) dt. \quad (11)$$

The fuel temperature used in Eq. (8) to evaluate the diffusion coefficient is expressed as

$$T(z) = T_0; t < 0$$

$$T(z) = T_0 + (T_m - T_0) \cos[2.4504(z - 0.5)]; t \geq 0 \quad (12)$$

where

- T_m = peak fuel temperature, °C
- T_0 = clad temperature, °C
- z = axial distance expressed as a fraction of the fuel length
- t = time after step increase in power.

It was assumed that the fuel temperature was invariant with radius. The hydrogen pressure over the zirconium-hydride surface when equilibrium prevails is strongly temperature dependent, as shown in Fig. 11, and for $ZrH_{1.65}$ can be expressed by

$$P_e = 2.59 \times 10^9 \exp[-1.997 \times 10^4 / (T + 273)] \quad (13)$$

The coefficients have been derived from the data developed by Johnson.¹³ The rate at which hydrogen is released (or reabsorbed) takes the form

$$g(t, z) = \left[\frac{P_e(z) - P_h(t)}{P_e(z)} \right] f(t, z) \quad (14)$$

where

- $f(t, z)$ = derivative given in Eq. (9) with respect to time evaluated at the axial position z
- $P_h(t)$ = hydrogen pressure in the gap at time t
- $P_e(z)$ = equilibrium hydrogen pressure at the ZrH temperature at position z .

The internal hydrogen pressure is then

$$P_h(t) = 1.406 \times 10^3 (T_0 + 273) \int_0^t \int_0^1 g(t, z) dz dt \quad (15)$$

This equation was approximated by

$$P_h(t_i) = 1.406 \times 10^3 (T_0 + 273) \times \sum_{i=1}^m \sum_{j=1}^m \left[1 - \frac{P_h(t_{i-1})}{P_e(z_j)} \right] f(t_i, z_j) \Delta z \Delta t \quad (16)$$

where the internal summation is over the fuel element length increments and the external summation is over time.

In Fig. 13 the ratio of the fuel element internal pressure to the equilibrium hydrogen pressure is plotted as a function of time after a step increase in temperature. The maximum fuel temperature

is 1150°C for which the equilibrium hydrogen pressure in $ZrH_{1.65}$ is 2080 psi. The calculation indicates, however, that the internal pressure increases for ~0.3 sec at which time the pressure is ~420 psi, or ~20% of the equilibrium value. After this time, the pressure slowly decreases as the hydrogen continues to be redistributed along the length of the element from the hot regions to the cooler regions. Calculations were also made for step increases in power to the peak fuel temperatures of 1250 and 1350°C. Over this range the time to the peak pressure and the fraction of the equilibrium pressure value achieved were approximately the same as for the 1150°C case. Thus, if the clad remains below ~138°C, the maximum internal pressure that would produce the yield stress in the clad is 1025 psi [see Eq. (2)], and the corresponding equilibrium hydrogen pressure could be 5 times greater, or ~5000 psi. From Eq. (14) (or Fig. 11) this pressure corresponds to a maximum fuel temperature of ~1240°C in $ZrH_{1.65}$. Similarly the equilibrium hydrogen pressure could be 5×1840 or 9200 psi before the ultimate clad strength was reached corresponding to a fuel temperature of ~1300°C.

The ultimate strength of the clad decreases slowly to ~57,000 psi at 500°C. Since the pressure is a strong function of fuel temperature the fuel temperature to produce rupture decreases very slowly over this range, remaining at 1300°C for a clad temperature up to 300°C and decreasing to ~1293 and 1282°C for clad temperatures of 400 and 500°C, respectively. For nongapped TRIGA pulsing fuel elements, the clad temperature during heat flow from a pulse is greater than the 138°C ACPR value but normally <500°C.

Measurements of hydrogen pressure in TRIGA fuel elements during steady-state operation have not been made. However, measurements have

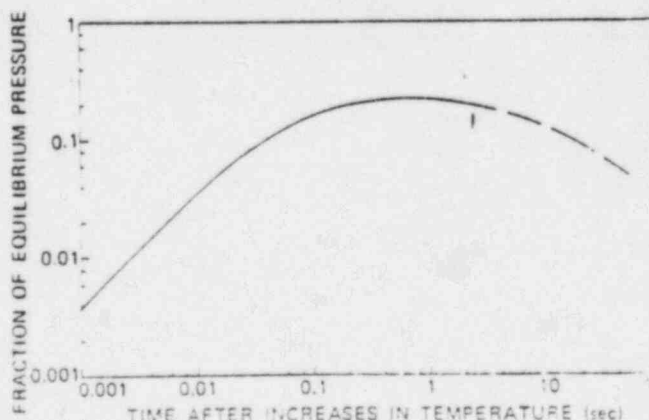


Fig. 13. Fuel element internal pressure versus time after a step increase in maximum fuel temperature.

been made during transient operations and compared with the results of an analysis similar to that described here. These measurements indicated that in a pulse in which the maximum temperature in the fuel was $>1000^{\circ}\text{C}$, the maximum pressure was only $\sim 6\%$ of the equilibrium value evaluated at the peak temperature. Calculations of the pressure resulting from such a pulse using the methods described above gave calculated pressure values ~ 3 times greater than the measured values.

An instantaneous increase in fuel temperature will produce the most severe pressure conditions. When a peak fuel temperature of 1150°C is reached by increasing the power over a finite period of time, the resulting pressure will be no greater than that for the step change in power analyzed above. As the temperature rise times become long compared with the diffusion time of hydrogen, the pressure will become increasingly less than for the case of a step change in power. The reason for this is that the pressure in the clad element results from the hot fuel dehydrating faster than the cooler fuel rehydrides (takes up the excess hydrogen to reach an equilibrium with the hydrogen over-pressure in the can). The slower the rise to peak temperature, the lower the pressure because of the additional time available for rehydriding.

An assessment of the effect of some of the assumptions used in this analysis is given below:

1. It was assumed that the peak fuel temperature was constant with radius for evaluating the hydrogen diffusion coefficient. This overestimates the average fuel temperature by $\sim 15\%$. As the diffusion rate is very temperature sensitive, this assumption provides a degree of conservatism considerably in excess of 15% .

2. The diffusion model used does not rigorously account for the changing boundary condition imposed by the hydrogen confined to the fuel-clad gap. The modification to the model to account for diffusion back into the fuel is an approximation that is reasonable as long as the total fraction of the hydrogen lost from the fuel is small. At the time of the maximum hydrogen pressure, this fraction was calculated to be $\sim 6 \times 10^{-4}$. From this it is concluded that the model should be valid in calculations of maximum pressure.

The foregoing analysis gives a strong indication that the clad will not be ruptured if fuel temperatures are never greater than in the range 1240 to 1300°C , providing the clad temperature is $<500^{\circ}\text{C}$. However, a conservative safety limit of 1150°C has been chosen for this condition. As a result at this safety limit temperature the pressure is at least a factor of 5 (and up to a factor of

18) lower than would be necessary for clad failure. A factor of 5 is more than adequate to account for uncertainties in clad strength and manufacturing tolerances. The integrity of the clad has been demonstrated by TRIGA reactor pulse experiments to fuel temperatures $\approx 1150^{\circ}\text{C}$.

Under any condition in which the clad temperature increases above 500°C , such as during a loss-of-coolant accident or under film boiling conditions, the temperature safety limit must be decreased as the clad material loses much of its strength at elevated temperatures. To establish this limit, it is assumed that the fuel and the clad are at the same temperature. An analysis for this condition indicates that at a fuel and clad temperature of $\sim 950^{\circ}\text{C}$, the equilibrium hydrogen pressure produces a stress on the clad equal to its ultimate strength. There are no conceivable circumstances that could give rise to a situation in which the clad temperature was higher than the fuel.

The same argument about the redistribution of the hydrogen within the fuel presented earlier is valid for this case also. In addition at elevated temperatures the clad becomes permeable to hydrogen. Thus, not only will hydrogen redistribute itself within the fuel to reduce the pressure, but some hydrogen will also escape from the system entirely.

The use of the ultimate strength of the clad material in the establishment of the safety limit under these conditions is justified because of the transient nature of such accidents.

CHARACTERISTIC PERFORMANCE VALUES FOR A TRIGA ACPR

The core characteristics projected for the performance of the TRIGA ACPR containing fuel optimized for pulsing operation are given in Table II.

The standard experiment used for the analysis of the system, with other than air in the irradiation region, consisted of a mixture of $37.5 \text{ vol}\% \text{ CH}_4$, $12.5 \text{ vol}\% \text{ stainless steel}$, and $50 \text{ vol}\% \text{ void}$, which was homogenized to fill the volume of the test cavity. This standard experiment was convenient in that it had a calculated reactivity worth ($\sim \$5.50$) in the range of interest as the design upper limit for routine experiments. The final recommended reactivity worth upper limit for routine experiments is set equal to the reactivity insertion necessary to produce a 1000°C maximum fuel temperature.

Note from Table II that the reactivity insertion necessary to produce a 1000°C maximum temperature is about the same whether an experiment is in the experimental cavity or not. These values

TABLE II
Pulse Performance and Parameters for ACPR

Parameter	Value	
Fuel material	12 wt% U-ZrH _{1.6} (U is 20% enriched)	
Critical mass	117 elements, includes 6 fueled followers, 6.20 kg ²³⁵ U	
Operational loading (~\$10 excess reactivity)	154 fuel elements, 6 fueled followers, up to 5 aluminum-followed pulse rods	
Worth of pulse rods	\$4.80 (min)	
Worth of bank rods	\$8.15 (min)	
Effective delayed neutron fraction (β_{eff})	0.0073	
Prompt neutron lifetime (l)	32 μ sec	
Prompt temp coefficient (α) (average between 23 and 700°C)		
Air in irradiation hole, 6 bank rods half in, pulse rods out	$-9.6 \times 10^{-5} \delta k / \Delta T$	
Standard experiment in irradiation hole, all rods out	$-9.3 \times 10^{-5} \delta k / \Delta T$	
	Irradiation Region Contents	
	Air	Standard Experiment
\bar{P}/\bar{P}		
Axial	1.25	1.25
Radial (power in T_{max} cell/ \bar{P} core)	1.07	0.98
Cell	1.76	1.76
Total	2.35	2.16
Performance		
Peak adiabatic fuel temp (°C)	1 000	1 000
Average adiabatic core temp (°C)	555	590
δk (\$)	4.80	4.90
Core energy release (1 sec) (MW-sec)	106	115
n/cm ² >10 keV in hole (175 MeV/fission)	1.20×10^{15}	---
Peak power (MW)	20 000	22 000
Min period (msec)	1.2	1.2

are calculated to be about the same because, even though the temperature coefficient is somewhat reduced with the standard experiment in the irradiation region (actually a result of the bank rods being withdrawn to compensate for the standard experiment worth), there is a compensating change in the peak-to-average power generation in the core.

Figures 14 through 17 present the values of an analysis for a \$4.80 reactivity insertion followed by a scram. The total reactivity is added within 0.085 sec, with the rate at each point in time

reflecting the acceleration of the rod. The scram occurs 1 sec after the pulse begins.

Figure 14 shows the reactor power and energy release as a function of time after the initiation of the pulse. The maximum power is 20 000 MW, the prompt burst energy release (~0.1 sec) is ~100 MW-sec, and, within 1 sec, 106 MW-sec energy has been released.

Figure 15 illustrates the maximum fuel temperature in the fuel element in which the power density is greatest, the maximum clad temperature, the coolant velocity, and the maximum heat

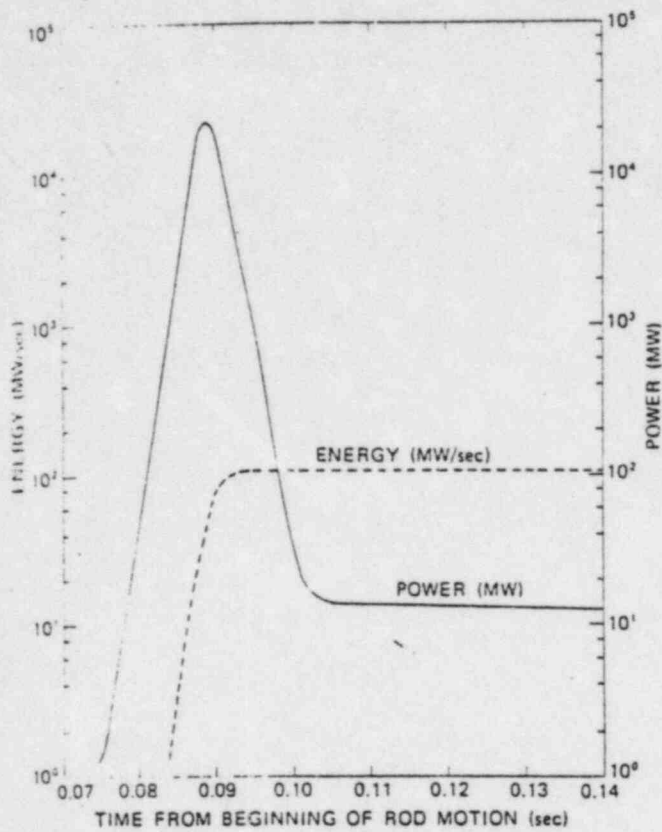


Fig. 14. Standard pulse power and energy released versus time from first rod motion.

flux. The peak fuel temperature of $\sim 1000^\circ\text{C}$ occurs 0.1 sec after the beginning of the pulse and quickly falls off to 880°C within 1.0 sec. The clad temperature does not begin to increase significantly until 0.5 sec after pulse initiation, at which time it begins to increase to its maximum value of 180°C at ~ 10 sec. This also corresponds to the time of the maximum heat flux, 12.5 W/cm^2 , and maximum coolant velocity, 220 kg/h .

Figure 16 shows the temperature at the thermocouple location as a function of time after the

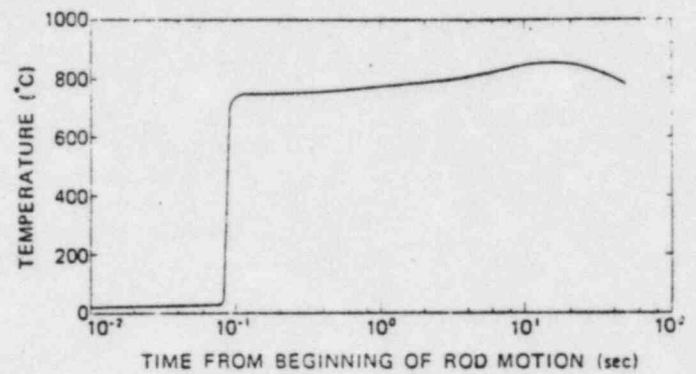


Fig. 16. Standard pulse thermocouple temperature for maximum power cell versus time from first rod motion.

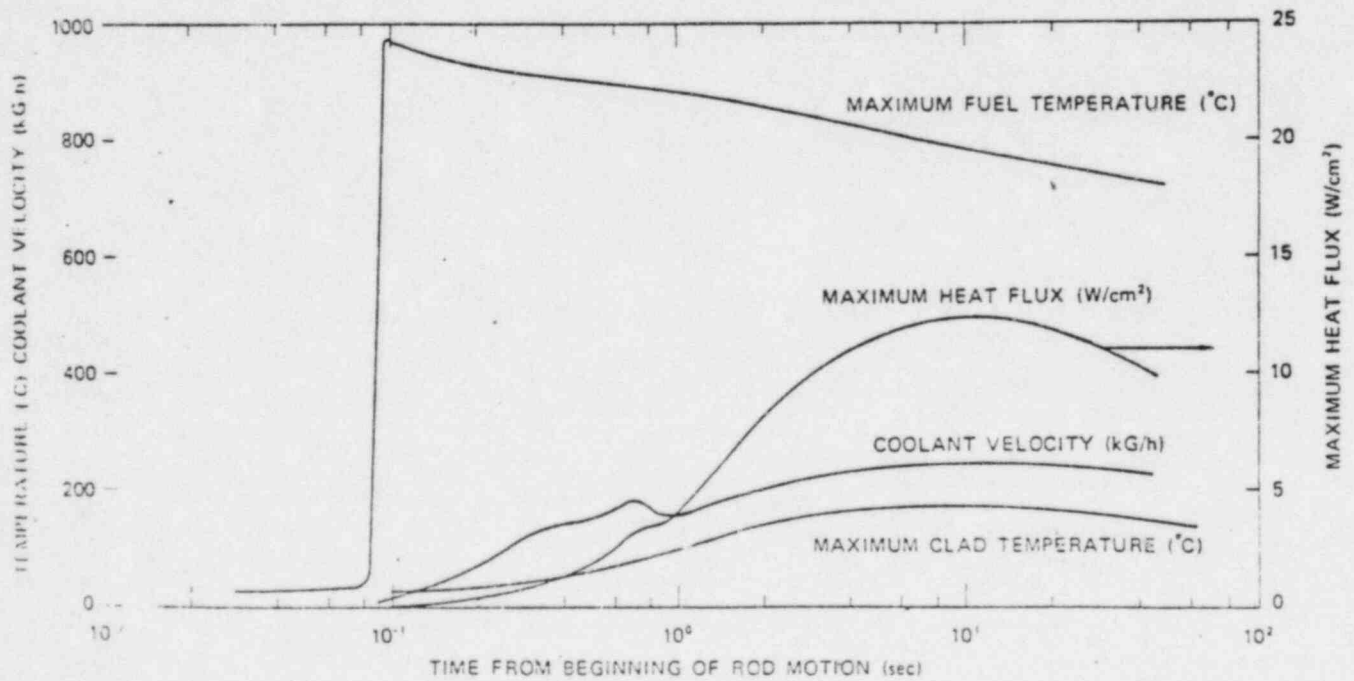


Fig. 15. Standard pulse hot-element at core edge—maximum temperature, heat flux, and coolant velocity versus time from first rod motion.

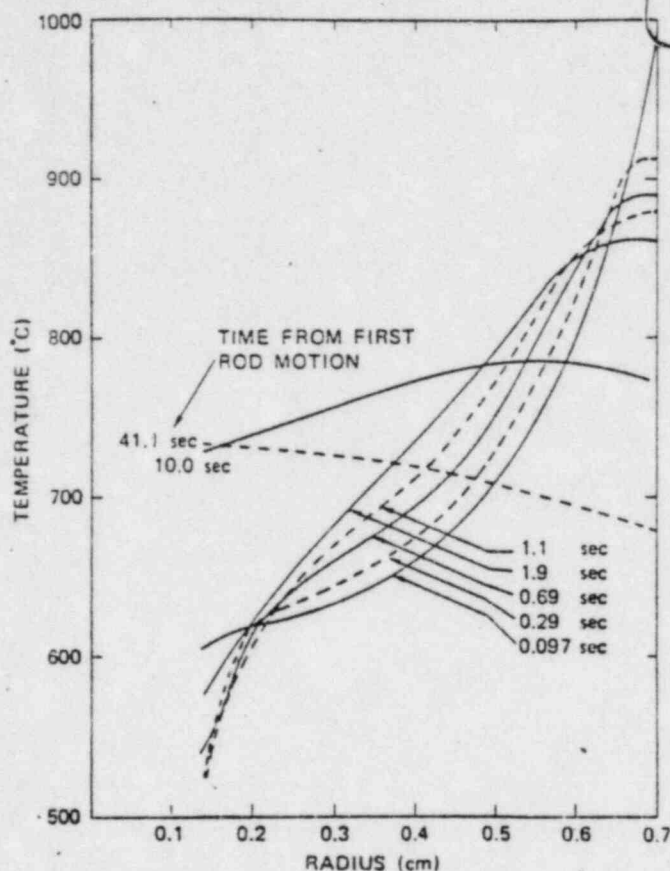


Fig. 17. Standard pulse radial temperature distribution in a fuel rod at core edge at various times from first rod motion.

beginning of the pulse. It rises rapidly as the prompt energy is deposited in the fuel and increases to its peak value (845°C) at ~15 sec.

The redistribution of the energy within the fuel is shown in Fig. 17. Here the temperature is plotted as a function of radial position (at the axial centerline) for several times after the pulse initiation. At 0.097 sec, the temperature distribution reflects, essentially, the adiabatic energy deposition in the pulse. By 0.29 sec, a significant amount of heat has flowed toward the clad and the fuel center (particularly the unheated zirconium rod). Within 40 sec, the initial distribution has been erased and conduction to the coolant dominates.

Comparisons of calculated performance, using parameters equivalent to those in this analysis, with experimental values from the Sandia Laboratories ACPR have generally shown agreement within ~5%, with the calculated values always being larger, and with the calculated peak power ~20% larger than the experimental value. The manner in which the kinetics parameters are interrelated would indicate that no single calculated value is greatly different from the real

value. They can all be at varying, relatively small differences from the real values, but the combination of errors for the quantities as they are related to influence the pulsing performance is within a reasonable accuracy for the measurements of the pulsing performance.

For a comparison of flux in the experimental cavity, Sandia quotes 1.55×10^{15} n/cm² (>10 keV) for a \$4.40 pulse. Using a 100-MW-sec energy release, to include energy beyond 10 sec that could be measured by the detectors, the flux-watt (>10 keV) is 1.55×10^7 . This compares to the computed value of $\sim 1.1 \times 10^7/W$. The calculated flux value assumes an energy release during fission of 190 MeV/fission. If the delayed energy from fission is not included, as would be the case during pulsing, the calculated neutron fluence value could increase by as much as 10%.

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ATTACHMENT 8

AFFIDAVIT
OF
JOSEPH A. SHOLTIS, JR.

Joseph A. Sholtis, Jr., being duly sworn according to law, deposes and says:

The Intervenor has on several occasions stated that repetitive pulsing during a LOCA is necessary to achieve Intervenor's postulated sudden temperature elevation such that multiple clad failures would result. (See page 128 lines 2 through 9, inclusive and page 115 lines 2 through 11, inclusive, of the transcript of Dr. Stillman's deposition in New York on 18 Dec 1982; see also Intervenor's responses to Licensees first-round interrogatories 33b, 24d, and 24e.; also see Intervenor's response to Licensee's first-round interrogatory #24f where Intervenor states, "If the 1 MW TRIGA reactor was not capable of the pulsing operation, it is unlikely that cladding failures would result from a LOCA involving that reactor.") It should be noted that this last statement by Intervenor has important implications to Contention 2, Accidents II.4 as well as this subject contention.

The important question is whether repetitive pulses can indeed be fired frequently enough during a LOCA and result in adequate temperature elevation for clad failures to occur. Licensee submits that pulsing during a LOCA, particularly repetitive pulsing with a frequency between pulses of a fraction of a second (the frequency which Intervenor has stipulated during Dr. Stillman's deposition in New York on 18 Dec 1982 -- see page 74 lines 11 through 14, inclusive, of the transcript of Dr. Stillman's deposition in New York on 18 Dec 1982), simply is not possible in the AFRRRI reactor. First, the only way in which the core could possibly become uncovered with water is if a rupture of the tank occurs at an elevation below the top of the core. (Note: Because of the higher elevation of all plumbing associated with the primary coolant system, the core physically cannot become uncovered

via drainage or pumping through breaches in primary coolant lines.) Upon loss of approximately 4-6 inches of water from the normal pool water level, a scram signal will be automatically initiated by actuation of the pool water level float switch. This action would terminate any power operation that happened to be in progress already and would also preclude any subsequent reactor power operations from taking place. Therefore, operation of the reactor during a pool water loss situation would require a total malfunction of the pool water level float switch such that a scram signal is not generated. Even if this malfunction were presumed to occur during a pool water loss situation and reactor power operation was also presumed to occur during the water loss, radiation alarms would alert the operator to an off-normal situation well before the core actually becomes uncovered since less and less water shielding would be available as the water loss progresses and direct gamma shine from the core would become evident. At this point, to recapitulate the scenario, water is being lost from the pool at a maximum rate of 250 gals/min (the rate which Intervenor stipulates in its response to Licensee's first-round interrogatory #33a and which is reiterated on page 45 lines 3 through 10, inclusive, of the transcript of Dr. Stillman's deposition in New York on 18 Dec 1982) which equates to a water level drop rate of approximately 4 inches/minute, the pool water level float switch has been presumed to fail such that no scram signal is generated, reactor power operations are presumed to take place during the water loss, and numerous audio-visual radiation alarms sound due to direct gamma shine before the core actually becomes uncovered. It is hard to believe that an operator would continue reactor power operations in the face of numerous radiation alarms sounding, or conversely, that the radiation detection system would fail totally and not provide alarms during a pool water loss situation. Nevertheless, we will still assume that reactor power operations continue to be performed as the water loss progresses toward ultimate core uncover;. At this point, a discussion of how pulses are fired at AFRRRI is necessary before continuing.

First, pulses are fired from a low power steady state condition, usually at approximately 15 watts but certainly never above 1 kilowatt. In fact, the AFRRRI reactor has a built-in interlock system which prevents firing the transient control rod out of core if the power level is greater than or equal to 1 kilowatt. Therefore, in addition to the already mentioned safeguards which must be presumed to fail in order to operate, this 1 kilowatt interlock must also be assumed to be non-operational in order to permit the firing of successive repetitive pulses. This is so because the power level of the reactor immediately after a pulse is fired and continuing for about ten minutes thereafter will always be greater than 1 kilowatt because of delayed neutrons produced from the pulse which ultimately die away on a negative 80 second period. Next, let's assume that the operator has attained a steady state power level of 15 watts by virtue of having manually withdrawn the three standard control rods. Typically to attain a 15 watt steady state power level at AFRRRI the "shim" and "safety" control rods must be fully withdrawn while the "regulating" control rod is withdrawn approximately 80%. At this point, the transient control rod anvil is raised (without any air supply to the transient rod drive) to the desired withdrawal point, the range select switch is turned to the "3 MW-pulse" setting and the mode select switch is turned to the "pulse-hi" or "pulse-lo" setting (at which point, the "pulse-fire" button will light up if the 1 KW interlock is satisfied; then, by depressing the "pulse-fire" button a pulse can be fired). Upon depressing the lit "pulse-fire" button, a pulse timer, which is normally set at 0.5 seconds, begins counting as the transient control rod is driven upward (to meet the anvil stop) which initiates the pulse. It takes approximately 100 msec for the transient control rod to reach its upper limit of travel when the lit "pulse-fire" button is depressed and it takes an additional 100 msec (maximum) for the pulse to occur and shut itself off via the action of the negative temperature coefficient of reactivity. When the pulse timer finally reaches 0.5 seconds or 500 msec after pushing the lit "pulse-fire" button (i.e. approximately 300 msec, minimum, after the pulse is already over), a signal is generated automatically

which scrams all the control rods which consequently fall back into the core. Once the standard control rods are back in the core (i.e. after approximately 500 msec after the pulse timer initiates the scram signal), the standard control rod drives begin driving "down" automatically to meet the already inserted control rods and this automatic lowering action, in and of itself, takes approximately 30 seconds. Once the standard control rod drives are fully "down" and again in contact with the standard control rods, then and only then can the standard control rods be manually withdrawn again in preparation for a second pulse. It takes approximately 3 minutes to manually drive these control rods back out of core to reestablish a steady state power condition. What all this means is that if the pulse timer is operational, it is impossible to fire successive pulses at a frequency faster than about one every 4 to 5 minutes and this relatively quick pulse repetition rate can only be achieved if the 1 kilowatt interlock is non-operational and operator error is also assumed. In order to fire repetitive pulses faster than one every 4 to 5 minutes, the 1 kilowatt interlock must fail, the pulse timer must also fail to scram the control rods and gross operator error must additionally be involved. And even for this incredible series of events, the pulse repetition rate could be no faster than one every 600 msec. That is, it takes physically about 500 msec for the transient control rod to drop back into core after the first pulse is initiated and an additional 100 msec to drive it back out for the second pulse. This raises an interesting question. If the pulse timer must fail to initiate a scram signal in order to be able to fire successive pulses every 600 msec, then how does the transient control rod get back into the core for firing it out the second time. This demands not only a malfunction of the pulse timer but a selective and particular malfunction of the pulse timer which somehow leaves the standard control rods remaining withdrawn but nevertheless scrams the transient control rod so that it can be redriven out of core for the second and subsequent pulses. All of this actually becomes rhetorical anyway since the second and subsequent pulses will not occur even if the transient control rod could be driven selectively in and out of core at will at a

very fast rate. The reason subsequent pulses will, in fact, not occur is due to the extremely large amount of negative reactivity that is introduced as a normal matter of course as a result of the first pulse and its associated fuel temperature increase; this temperature increase occurs and persists for several to tens of seconds. Therefore, even though it might be possible to selectively drive the transient control rod in and out of the core at will at a very high repetitive rate, the core will still be well subcritical (many dollars subcritical) as a result of the first pulse and the temperature heat-up which occurs and persists. Therefore, the transient control rod worth will be insufficient to overcome the core's large subcriticality to even attain criticality (let alone fire a second pulse) even if the transient control rod could indeed be selectively driven out again immediately after the first pulse.

Let's assume AFRRI fires a \$3.28 pulse. Such a pulse will result in a peak power of about 2500 MW(t) and a fuel temperature rise of approximately 550°C. The pulse will have a width at its half-maximum power level of approximately 10 msec and the temperature increase will decrease with time but persist at significant levels for approximately 10 seconds or longer. Such a pulse would introduce \$9.90 of negative reactivity, since the prompt negative temperature coefficient of reactivity has a value of -1.8¢ for every 1°C of temperature rise. Therefore, since the transient control rod must be presumed to selectively scram immediately after the pulse (in order to permit subsequent rapid withdrawal of the transient control rod), the core would be \$9.90 below the delayed critical state at the time the 550°C fuel temperature rise actually was attained and would decrease slowly to a zero value over the following approximately 10 second or longer time interval. Since AFRRI's transient control rod total integral reactivity worth is only \$3.35, even if it could be driven out of core totally and immediately after the first pulse, the core would still be \$6.55 below the delayed critical state. This means that even though safeguards and the operator might fail and permit selective and frequent repetitive transient rod firing in and out of core, successive pulses could not actually be fired more rapidly than about one every

ten seconds (but certainly not on the timeframe of fractions of a second as intervenor has stated) and at these limited rates, the fuel temperatures would have (and, in fact, must have) recovered to near ambient conditions.

Up to this point, nothing has been said about the feasibility of firing a single or multiple series of pulses with the core partially or completely uncovered as a result of the presumed pool water loss. Intervenor has admitted that criticality cannot even be attained if the core is completely devoid of water. (See page 115 line 15 through page 116 line 3, inclusive of the transcript of Dr. Stillman's deposition taken in New York on 18 December 1982.) Further, when asked by Licensee, "Can you give us some feeling about how much water must be in the core to still be able to attain criticality and fire a pulse?", Dr. Stillman indicated that Intervenor had performed such a calculation, viewed it as an essential point, and that this information would be forthcoming. (See page 116 lines 4 through 14, inclusive, of the transcript of Dr. Stillman's deposition taken in New York on 18 Dec 1982.) To date, this vital information has not yet been provided to the Licensee. Without this information, Licensee is at a loss as to how criticality could be attained and a pulse could be fired when the core is uncovered to any extent. Licensee must therefore imagine on its own the arguments which substantiate Intervenor's claim that criticality and pulsing can occur during a LOCA without benefit of any "insight" from the Intervenor.

First, it should be recalled that a low power steady state condition is first established by the manual withdrawal of the standard control rods in preparing to fire a pulse. It was also pointed out that to attain an approximate 15 watt steady state power level in anticipation of firing a pulse the "safety" and "shim" control rods had to be fully withdrawn while the "regulating" rod had to be withdrawn approximately 80%. This leaves only the upper 20% of the regulating rod (or about 30¢ of reactivity) which could be used to overcome any negative

reactivity as a result of a water void in the core and still permit the firing of a pulse from the steady-state condition. This is true since the transient control rod must be fully "down" to initiate a pulse. This 30¢ of reactivity available in the regulating rod would be completely used up if only approximately 3-5% of the core water was missing. We might, therefore, only fire a pulse (any pulse) when the core is provided with 95% (or more) of its total normal water inventory.

However, even this limit is open to question. The interstitial water between the fuel elements in core is necessary as a moderator to ensure that neutrons, in fact, reach thermal energy (where fission predominantly occurs) effectively. When water is removed, neutrons cannot reach thermal energy and this is true wherever water is presumed to be missing. Therefore, if we assume, for example, that some fraction of the upper portion of the active core region were devoid of water, then the uncovered fueled region of each fuel element that is devoid of water has no input of thermal neutrons to initiate fission. This means that all uncovered fuel element regions will be largely incapable of effectively contributing to fission, power, neutron population, fission density, and thus even a fuel temperature increase since inadequate neutron moderation is provided to such uncovered regions. This indicates that locally within the core wherever water is not provided, conditions cannot be aggravated beyond those conditions already in existence because of the water loss or LOCA by itself. It should also be pointed out that each fueled region of the core which is presumed to be devoid of water will have an associated higher than normal fuel temperature because of the missing water together with the internal source of heat being generated by virtue of the in-place fission products that are undergoing natural radioactive decay, and this increased fuel temperature locally will automatically introduce negative reactivity (also locally) which would also act to suppress effective fission in those uncovered regions.

This discussion indicates that attainment of criticality, by itself, would be seriously in question even for relatively small or minor water void fractions. And certainly if criticality is not possible then pulsing (even the firing of a single pulse) would be totally out of the question.

In summary, Licensee has demonstrated that numerous safeguards must fail (extremely unlikely) and gross operator error must be assumed to permit the transient control rod to be selectively fired repetitively out of core during a LOCA. Even if all of this were presumed to occur, actual successive pulses could not, in fact, occur at a frequency faster than one approximately every 10 seconds, i.e., until fuel temperatures have basically recovered to ambient conditions. Licensee further has demonstrated that uncovered fuel regions cannot effectively contribute to fission. Thus, fuel temperatures in these uncovered regions cannot become aggravated beyond those conditions which already exist by virtue of the water loss alone. Licensee submits, therefore, that multiple or even single clad failures during a LOCA are not expected since conditions necessary for clad failure cannot be attained. The record amassed to date, particularly the analyses of the LOCA as provided in the AFRRRI SAR and as formerly provided in the 1965 license amendment LOCA analysis, are valid and provide a true, realistic, and, in fact, conservative picture of such an occurrence (i.e., a presumed LOCA at AFRRRI).



JOSEPH A. SHOLTIS, JR.

Sworn to and subscribed before
me on this 25th day of Feb, 1983.

My commission expires:
April 8, 1984
Miriam E. Lesley
Notary Public

UNITED STATES OF AMERICA
NUCLEAR REGULATORY COMMISSION

BEFORE THE ATOMIC SAFETY AND LICENSING BOARD

In the Matter of

ARMED FORCES RADIOBIOLOGY
RESEARCH INSTITUTE

(TRIGA-Type Research Reactor)

Docket No. 50-170

(Renewal of Facility
License No. R-84)

CERTIFICATE OF SERVICE OF DUPLICATE SIGNED
COPIES OF 25 FEBRUARY 1983 FILING

I hereby certify that true and correct copies of the foregoing "LICENSEE'S MOTION FOR PARTIAL SUMMARY DISPOSITION" were mailed this 25th day of February, 1983, by United States Mail, First Class, to the following:

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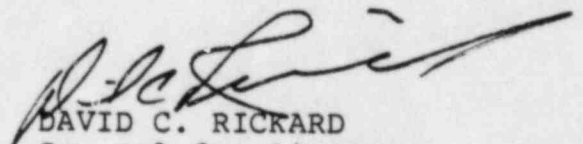
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