proceedings of the
FAST
REACTOR
SAFETY
MEETING

held April 2-4, 1974, in Beverly Hills, California

presented by
ANS Technical Group for Nuclear Reactor Safety
Los Angeles Section of the American Nuclear Society

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This Conference on Fast Reactor Safety represents the first such conference sponsored by the American Nuclear Society and the first topical meeting sponsored solely by the Technical Group for Nuclear Reactor Safety. However, it is only one of a fairly long series of conferences on this subject, starting back with a meeting in Detroit in 1954, an IAEA Conference in Vienna in 1961, and the Argonne National Laboratory Conferences of 1963 and 1965. Since then, two conferences have been held at Aix-en-Provence, two in Karlsruhe and another at Argonne. The subject has also formed part of several other conferences.

A major purpose of the Technical Group for Nuclear Reactor Safety is to provide a forum for open and full discussion of the various aspects of reactor safety, in the belief that in this way the public health and safety can best be served. The Technical Program Committee wishes to express its thanks to the many authors who have presented papers at the conference; to the session chairmen for safeguarding the extensive discussion periods for their intended purpose of frank interaction among participants; to the panel members for their stimulating remarks; and to the host of others who worked to keep the Conference machinery operating.

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SESSION 11
SODIUM BOILING AND FLOW BLOCKAGE
Chairman: G. Pincheva (CNEN-Casaccia)
Sodium Boiling Experiments and their Importance for the Reactor Safety

K. Gast, W. Peppler, D. Smidt
Institut für Reaktorentwicklung
Kernforschungszentrum Karlsruhe

Abstract

Within the scope of the R and D program of the LMFBR’s (SNR) the behavior of local and integral cooling-disturbances in subassemblies is investigated up to boiling. As starting conditions these processes are of great interest with respect to the following safety problems: the failure propagation and the course of severe hypothetical accidents.

A short survey is given on the different safety requirements with respect to the national reactor concepts. The Karlsruhe work on the problems of cooling disturbances, detection and sodium boiling is reviewed and future prospects are indicated.

1. Core Incidents

1.1 Loss of Cooling by Pump Failure or Subassembly Blockage

Within the framework of the SNR 300 licensing procedure the loss of flow incident initiated by pump failure and the simultaneous failure of all shutdown systems has been studied at Karlsruhe, in addition to accidents caused by reactivity ramps. Bulk sodium boiling [1], besides cladding failures by temperature, constitutes the first significant event in the development of the disturbance and has essential repercussions on the further development through:

- repercussion on the reactor power via the location-dependent positive and negative void coefficient, respectively,
- cladding tube melting and fuel slumping due to interruption of fuel element cooling.

Therefore, the goals followed in the program on gross boiling can be summarized as follows:

- Investigate the phenomena of bulk boiling in geometries resembling that of the fuel element.
- Investigate the time dependent development of the void taking into account the incipient boiling superheat of sodium.
- Realize the consequences of boiling - and condensation pulses with respect to fault propagation.
- Recognize typical signals to detect these events.

1.2 Cooling Disturbances Caused by Local Blockages

From an overall analysis of possible modes of local fault propagation in a LMFBR fuel subassembly [1, 2] the phenomena associated with localized sodium boiling downstream of a blockage within the fuel element bundle were identified as critical points in the safety assessment of the fault propagation problem. Local boiling could initiate a rather rapid growth of the fault by either one or combined effects of

- vapor blanketing, dryout and failure of fuel pins, possibly accompanied by the ejection of molten fuel,
- production of sufficiently large amounts of sodium vapor to interfere with the overall coolant flow through the subassembly, thereby leading to hydrodynamic instability and bulk sodium boiling.

Consequently, the main objectives of the local boiling program can be summarized as follows:
- Investigate the nature of the local boiling process.
- Demonstrate that local boiling behind a blockage large enough to be detected by monitoring the mixed mean temperature or flow at the subassembly outlet does not cause dryout and subsequent fuel melting.
- Demonstrate that local boiling behind a smaller blockage can be detected by flow variations at the outlet of the fuel element or by cross measurement of the boiling noise or neutron noise, respectively.

1.3 Influence of Various Reactor Concepts

Various safety concepts and construction lines of fast breeder reactors influence the safety investigations considerably, because it is attempted to simulate the particular reactor as well as possible. This leads to differences in the thermohydraulic data, the rod power, the time dependent mass flow rates, etc. As an example the conditions of the pump coast down shall be taken:

In the SNR 300 the boiling temperature is reached after 5 sec. at full rod power, in the FFTF, however, it is reached after 19 sec. This determines the axial temperature profile of the test section at the onset of boiling and has corresponding effects on the evaporation and condensation events and, ultimately, on the formation of the flow regime. Also the hydraulic conditions are different. The SNR subassembly has grid-type spacers and the FFTF subassembly helical wire spacers, the latter entailing lower pressure losses. Taking all the parameters together, boiling does not only start earlier in the SNR fuel element, but also transient boiling characterized by some few bubbles and liquid slugs will dominate. There is hardly any stable short-term boiling.

The problems encountered in the safety concept of the Phenix reactor are quite dissimilar. Due to the negative bowing coefficient the reactor power decreases considerably during coast down so that the boiling temperature is at
tained only after a longer period of time at reduced rod power. This favors a temporary boiling process which can be designated as quasi-stable boiling.

Although these differences cannot be described individually, they clearly show that the different flow regimes under transient boiling conditions observed behind disturbances, are partly due to the different initial conditions.

However, there are also discrepancies in the results, particularly relating to the incipient boiling superheat of sodium, which cannot be explained in this way.

2. Investigations of Flow Disturbances

2.1 Total Blockage and Pump Failure

Sodium boiling under this type of perturbation is a transient process which is mainly determined by:

- the thermophysical properties of the liquid metal such as good thermal conductivity, high density ratio of liquid to vapor, and the tendency to incipient boiling superheat,

- the geometrical conditions in the subassemblies, small hydraulic diameter together with a great length of the cooling channel,

- the high heat flux with small pressure drop at the fuel element inlet section. This has a particular impact on the stability of the two-phase flow.

Therefore, the boiling behavior could not be automatically transferred from other liquids to sodium. Boiling experiments had to be carried out with sodium under conditions as close as possible to reactor conditions. Parallel to this effort a theoretical model had to be established which should be applicable to disturbances under experimental and reactor conditions as well.

The tests are intended to give the following detailed information on the:

- type and development of void caused by boiling at constant heating power both for complete sudden blockage of the inlet and for pump coast down,

- influence of the incipient boiling superheat,

- cooling under transient boiling condition,

- time and location of the onset of dryout,

- details of liquid sodium re-entering the superheated channels,

- two-phase pressure drop of sodium,

- boiling and condensation pressure pulses,

- propagation of boiling events in a fuel element to neighbouring elements,

- testing of boiling detection methods,

- transferability of results obtained in the individual channel to subassemblies.

Technical difficulties, especially the simulation of fuel rods by electrical heaters, influenced the experimental procedure. Sufficient tests could be performed only out of pile. Under these aspects the realization of the test program was started in 1966 by several parallel activities [3-7]. An optimum simula-
tion of specific conditions present in the reactor was one of the most important design criteria.

In about 150 single runs, executed in narrow channels [6], the events taking place during sodium boiling until the onset of dryout were investigated under conditions typical of the subchannels of a fuel rod bundle. One of these test sections whose hydraulic diameter was between 4 and 12 mm is shown in Fig. 1. It consisted of a nickel tube with a displacement tube in the center. So an annular channel is formed with a hydraulic diameter of 4 mm. Compared to that the hydraulic diameter of the SNR fuel element subchannel is 5.09 mm. Surrounding the tube over the 500 mm long heated section an induction coil has been provided through which the high-frequency energy is coupled into the test tube. The tube was inserted in a sodium loop. In this configuration specific heat fluxes up to 700 W/cm² could be reached. Thermocouples, fast pressure transducers, and flowmeters indicated the events taking place in the test section. The important results and conclusions are explained with the help of some diagrams.

Fig. 2 gives an idealized view of the events under conditions of complete and sudden loss of flow initiated by quick closure of the valve at the inlet for a small incipient boiling superheat. The plot shows the position in time of the vapor-liquid interface (S), two pressure differentials related to the initial pressure in the heated $\Delta P_{\text{dyn}5}$ and in the condensation section $\Delta P_{\text{dyn}2}$ and some typical wall temperatures. The analysis of the various signals measured is summarized as follows:

- Sodium is ejected through a single bubble.

- Following the first ejection, sodium no longer enters the channel but reaches only the boundary zones of the heated section because it is prevented from advancing by the high pressure existing in the bubble ($\Delta P_{\text{dyn}5}$). So cooling by liquid reentry is ruled out.

- A film is left on the wall which maintains cooling over a short period of time. Nevertheless, the temperatures continue to rise in the center of the channel due to the high pressure drop in the bubble extending from the evaporation to the condensation surface.

- Even after the evaporation of the film, a vapor pressure is maintained by vaporization in the boundary zones of the heated section, which impedes noticeable reentry.

For comparison with the idealized representation Test No. 156 is shown in Fig. 3. The oscillation of the interface (S) distinctly reveals that the liquid sodium no longer enters into the heated section. The onset of dryout is clearly indicated by the temperatures m and k 0.5 sec after the begin of the test. No incipient boiling superheat was observed in this experiment.

Fig. 4 shows transient boiling with a superheat of 60°C. The ejection, i.e. the formation of the first bubble occurs at a higher rate due to the higher vapor pressure. Compared to the results obtained in the absence of incipient boiling superheat this idealized test differs in one essential point:

- The phase of film evaporation becomes shorter, since the high pressure drop in the bubble causing a higher two-phase velocity has a reducing effect on the film thickness.
A first theoretical investigation of these problems lead also to the analytical description of boiling events in the code BLOW 2 [8]. In this code an explanation is given also as to why only one or some few large bubbles are formed. This deviation from the familiar boiling behavior of water is due to the specific physical properties of the liquid metals, in particular their tendency to superheat. Schlechtendahl demonstrated that an incipient boiling superheat of $10^3{\text{C}}$ is still sufficient to keep nearly critical nuclei subcritical in the environment of a bubble for a sufficiently long period of time.

With the experimental results the code BLOW 2 was verified and preliminary parametric calculations performed on the course of the void in the reactor under boiling conditions. In Fig. 5 experimental results are compared with those of the code. The ejection represented by the course of the vapor-liquid interface is well reproduced by the model as long as the bubble has not yet reached the condensation surface. Then the code predicts an almost complete reentry into the heated section of the channel contrary to the experiment. There the liquid sodium does not reenter far beyond the heated section. This deviation is due to the neglect of the high pressure drop in the bubble between the evaporation and the condensation surfaces. The new code, BLOW 3, takes this into account together with other improvements.

The transferability of results from the individual channel to the entire rod bundle was studied both theoretically and experimentally. It could be expected from the high ratio of axial to radial pressure loss that a vapor - or gas-liquid interface would move rather uniformly in axial direction with respect to the cross section of the subassembly. This was also demonstrated photographically in a glass rod bundle, filled with a liquid of the same refractive index. In Fig. 6 the interface can be clearly recognized. In another large experiment called BEVUS 9[9]. Fig. 7, transient boiling events were investigated in a bundle with 169 electrically heated rods surrounded by 6 empty fuel element wrappers. Since the electric heaters could be operated only at about $10^0/0$ of the rating of the actual fuel rods, the bundle was heated up to high temperatures under pressure and then depressurized. In these experiments a flowmeter of Interatom intended for installation in the SNR fuel element outlet was also tested under boiling conditions.

Together with the results from the single channel experiments it was concluded that the propagation of cooling disturbances from a fuel element to neighboring elements can be ruled out for boiling processes.

Tests in the annular channel geometry were also executed under pump coastdown conditions. These are simulated with the EM-pump in about 20 tests. In Fig. 8 the pump coast down curves of the SNR and run No. 16/23 have been plotted for comparison. Boiling started at residual flows between 0.5 - 2.1 m/sec. The test parameters of the significant tests are shown in Table 1.

<table>
<thead>
<tr>
<th>Test No.</th>
<th>11, 12, 13</th>
<th>14</th>
<th>15</th>
<th>16, 19, 20</th>
<th>17, 21, 22</th>
<th>23</th>
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<th>25</th>
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<tbody>
<tr>
<td>System Pressure [bar]</td>
<td>0.78</td>
<td>0.78</td>
<td>1.17</td>
<td>1.17</td>
<td>1.17</td>
<td>1.57</td>
<td>1.57</td>
<td>1.5</td>
</tr>
<tr>
<td>Heat Flux [W/cm$^2$]</td>
<td>99</td>
<td>123</td>
<td>123</td>
<td>149</td>
<td>180</td>
<td>149</td>
<td>180</td>
<td>213</td>
</tr>
</tbody>
</table>

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The new code BLOW 3 [10] is a module of the larger program system CAPRI [11] being developed in Karlsruhe for analysing the dynamic behavior of fast reactors under accident conditions. Starting from the considerations underlying the first code, BLOW 2, and from the experience with boiling experiments, the theoretical model represents the two-phase flow as a sequence of the liquid slugs (Fig. 9) with idealized interfaces. The action of the model is explained with the example of a pump failure. It is distinguished between 3 different phases.

Phase 1

Boiling starts at the upper core end with the formation of a bubble, Fig. 10. Assuming incipient boiling superheat, this bubble hinders by the sudden pressure build up further bubbles to be generated and decelerates the following liquid. The expansion of this bubble, except for a thin liquid film at the fuel rods, fills the entire cross section of the fuel element and is determined by the inertia of the liquid columns and by after-evaporation of the liquid film. The bubble is transported to the blanket zone via the residual flow, where it condenses completely or in part. Due to the resulting pressure drop new bubbles are formed in the liquid entering the subassembly from below. By this mechanism of repeated formation of single bubbles accompanied by a flow delay or even a flow reversal of the following liquid, the boiling zone extends towards the core center. The flow regime present in this phase is comparable to that of the slug flow with the only difference that the thermal effects discussed and not the hydrodynamic effects are responsible for their generation.

Phase 2

If in the course of ejection a bubble extends axially over zones of strongly differing temperatures, a vapor flow is generated by evaporation of the liquid film at the hot surfaces and condensation of the vapor at the cooler surfaces. The flow changes into a flow regime comparable to two-phase annular flow. The vapor flow causes an axial pressure gradient and influences the ejection of the liquid sodium and the liquid film thickness.

Phase 3

The pressure fluctuations caused by alternating evaporation and condensation processes in the boiling zone lead to chugging of the liquid at the fuel inlet and outlet. However, after the first ejection the residual evaporation of the liquid film hinders the coolant from completely reentering into the core region. This implies dryout of the liquid film and hence complete local interruption of the fuel rod cooling.

One example is selected to compare the results of BLOW 3 with the experimental results. In case of a superheat higher than 40°C (Fig. 11, Test No. 23) the sodium is almost instantaneously ejected upwards and downwards from the test section by formation of a bubble. The upper and lower liquid-vapor interfaces oscillate at the same frequency and the liquid is hindered from re-entering the heated section through the evaporation of the sodium film. There is a good agreement between the calculations and the experiment. Continuous liquid slugs are not observed in this experiment starting with a superheat of about 40°C at the location of the onset of boiling. The observation made in earlier experiments was reconfirmed that boiling does not always start at the location of maximum superheat.
In the Experiment 24 (Fig. 12) a measurable superheat was not observed at the location of the onset of boiling. The slug type of flow can be clearly recognized. Flow regimes different from the mentioned ones were not found in these experiments. The difficulties encountered in reproducing in a model the transient boiling events which often develop stochastically appear from Table 2. It shows the conditions of dryout leading to the termination of the test.

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<tr>
<td>11</td>
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<tr>
<td></td>
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<td>n</td>
</tr>
<tr>
<td>13</td>
<td>1.2</td>
<td>i,k,l</td>
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<tr>
<td>14</td>
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<tr>
<td>15</td>
<td>0.9</td>
<td>l</td>
</tr>
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<tr>
<td>20</td>
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<td>n</td>
</tr>
<tr>
<td>21</td>
<td>1.05</td>
<td>m</td>
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<tr>
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<tr>
<td>23</td>
<td>1.25</td>
<td>o</td>
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<td>n</td>
</tr>
<tr>
<td>25</td>
<td>1.35</td>
<td>l,n</td>
</tr>
</tbody>
</table>

The intervals between the onset of boiling and location-dependent dryout vary between 0.1 and 1.6 sec. This cannot be explained by the test parameters, nor the occurrence of incipient boiling superheat.

Several separate studies [12, 13] of a fundamental character were concentrated on incipient boiling superheat which is typical of sodium. Although influencing parameters such as the material, purity of sodium, gas content, pressure history, surface finish and turbulence could be found, their interaction is still not clarified. It is not possible to predict incipient boiling superheat.

Another uncertainty exists with respect to the residual film thicknesses under conditions of transient boiling. The phenomenon of the residual film left after the onset of boiling was recognized early by film shots taken of boiling alcohols [14], but it is difficult to describe it in a physical model.

Experiments in sodium [15] performed in an induction heated test section of 9 mm diameter and 200 mm heated length under steady-state boiling conditions provided information about the film thickness and flow regime up to the critical heat flux as a function of the sodium vapor velocity. Some results are shown in Fig. 13. The film thicknesses were calculated also assuming pure annular flow (cf. solid plots). At vapor velocities of less than 200 m/sec they agree well with the experiments while the deviations become gradually larger beyond that value. This is explained by the preferred formation of annular mist flow at higher vapor velocities. The results of these experiments on the
critical heat flux are particularly important for boiling events behind local blockages.

2.2 Local Blockage

For the experimental investigation of local boiling full-scale bundles of electrically heated rods are needed. The heaters should allow cladding temperatures up to 1050°C at a rod power of 450 W/cm. Moreover, the rods must be assembled to closely packed bundles. Such experiments which call for considerable technical and financial efforts are being performed.

Preliminary experiments on local sodium boiling were, therefore, carried out in a simpler test configuration called "negative bundle" [16]. These investigations aimed at

- determining the transferability to sodium of temperature profiles measured in water behind blockages,
- assaying the type and development of local boiling events,
- verifying the validity of theoretical models,
- assessing the conditions leading to dryout,
- determining the detectability of local boiling events.

The test section consisted of an induction heated nickel tube of 21 mm inner diameter with a concentric displacement tube whose profile generated 16 sub-channels interconnected so as to form an annulus (Fig. 14). One can consider the test section as a two-dimensional bundle. The dimensions of the sub-channels correspond to those of the fuel element. 75% of the cross section is blocked by a plane plate simulating a flow obstruction at one of the grid spacers of the fuel element. This reduces cooling to such an extent that it generates major local increases in temperature and boiling of the coolant behind the blockage.

It was found in the experiments that despite turbulent flow there may be considerable incipient boiling superheat. Values in excess of 100°C were measured in 4 cases, but so far no systematic correlation with experimental parameters can be seen. From 21 boiling runs executed 12 showed no incipient boiling superheat.

The extent of superheat essentially determines the course of the boiling process in the initial phase. Without superheat (pattern 1) boiling starts with individual small bubbles. With decreasing sodium flow (which corresponds to an increase in size of the blockage) a steady increase of the variations of the velocity and a decrease of the bubble frequency can be stated, both indicating that larger bubbles with longer lifetimes are formed. The boiling process is quasi-stationary as long as no parameter is altered. A typical boiling pattern is shown in Fig. 15. This is a plot of the sodium velocity at the inlet and outlet of the test section, which represents the length of the bubbles calculated by integration of the velocities, and the pressure pulsations measured at the outlet of the test section as a function of time over 0.8 sec. The diagram clearly shows that single bubbles are generated. Most of them condense completely, as indicated by the abrupt increase of the outlet velocity, and the simultaneous pressure pulse (e.g., at 0.35 sec). Follow-on bubbles are generated without any time delay.
Fig. 16 shows an experiment with moderate incipient boiling superheat. Some characteristic temperatures, the sodium velocity, the length of the bubble, and the dynamic pressure are plotted vs. time. At 3.4 sec, a first bubble is generated at a wall superheating level of about 40°C which collapses completely 0.2 sec later. The energy stored in the wall as superheat is not yet completely removed by the first bubble so that a second but smaller bubble is formed.

After some 5 bubbles have been generated, an equilibrium is attained. Then, not only superheating of the wall is removed \( T_9 \), but also the temperatures outside the boiling region decreased as a result of the pumping action of the expanding and collapsing bubbles \( T_{10} \). Subsequently, the course of the experiment converges into that without superheat already described.

No interruption of surface cooling due to complete evaporation (dryout) of the sodium film generated during bubble formation was observed in the pattern 1 and 2 experiments. On the contrary, cooling of the blocked region was intensified as soon as local boiling had started, as indicated by the decrease of the temperatures in Fig. 16.

Before comparing the experimental results with those predicted by the local boiling model developed by K. Gast \([2]\), a short description of this model shall be given. It is reasonable to assume \([17, 18]\) that the saturation temperature will be reached first in the wake downstream of the blockage. Because of the steep temperature gradients in the wake and the tendency of sodium to initiate boiling at liquid superheat, only one bubble is supposed to grow at a time. When the bubble originates, its vapor pressure exceeds the ambient pressure by an amount corresponding to liquid superheat. The dynamics of the bubble depends on its internal pressure and on the inertia and friction forces of the liquid to be displaced. According to a proposal by Schlechtendahl \([8]\), the bubble pressure is calculated from the location-dependent temperature distribution at the onset of boiling and hence is only dependent on the size of the bubble.

The non-steady state energy and mass transport processes within and outside the bubble are not taken into account. Because of the liquid inertia, the bubble grows beyond its "equilibrium" size. This small "overswing" into the subcooled region is sufficient to cause its pressure to become much lower than the pressure that would prevail under steady-state flow conditions. The bubble, therefore, starts to collapse. An isothermal process is assumed for bubble condensation.

The model was applied to the experiments with incipient boiling superheat (pattern 2). Calculation and measurement agreed satisfactorily for the first bubble. The model, however, cannot be applied to the following bubbles because the temperature distribution is not known at the moment of collapsing of the first bubble and the formation of the second. The experiments showed no time delay between the bubbles as expected by the assumption of isothermal condensation in the model. This deviation might partly be due to the specific thermohydraulic properties and the geometry of the test section.

Although, initially, condensation is isothermal, it becomes adiabatic in its last phase. This causes an increase in the bubble temperature so that a new bubble can be generated without delay.

More recent measurements with water have shown that temperature profiles are generated behind local blockages in rod bundles, which are qualitatively
comparable to those in the test section [18], with a broad maximum immediately behind the blockage and a nearly linear decrease in the axial direction. Calculations on local boiling in the fuel element with these temperature distributions show that the condensation temperature is only a little below that of the equilibrium-saturation temperature so that also in this case only short waiting times or none at all must be anticipated.

Because of the low thermal capacity of the cladding tubes one bubble is sufficient to remove the superheat energy. Hence, the chains of bubbles observed in the pattern 2 experiments are not to be expected in the fuel element.

Concerning dryout, the experimental conditions are conservative, because of the higher evaporation rates due to the higher thermal conductivity of nickel, the material of the test section. It is to be anticipated that under local boiling conditions dryout will not occur in the fuel element provided the geometry is largely intact.

2. 3 Detection of Blockages

Of the possible boiling detection methods only the flowmeter at the fuel element outlet and test section, respectively, will be treated here. It was shown that local boiling is a process involving one single bubble, even in case there is no superheat at the onset of boiling (Fig. 15). The resulting movements of the liquid superimpose the average velocity and lead to characteristic oscillations. In the experiments amplitudes of more than 100 \%/o of the mean value were measured. Generally, the expected signal depends on the mean velocity, the bubble size and the bubble life.

More detailed information was provided by the frequency spectrum of the signals. Fig. 17 shows for 3 runs the spectral power density distributions of the velocity measured at the outlet of the test section, without boiling in the upper diagram, with incipient boiling in the medium diagram and with intense boiling in the lower diagram. The changes of the spectrum caused by boiling can be clearly recognized. In addition to the bubble frequency (about 7 Hz in the lower diagram) influences exist in the whole region considered up to 80 Hz. The relative velocity fluctuations to be expected in SNR due to local boiling were calculated by Gast [2] and found to may exceed 100 \%/o.

Starting from stagnant sodium, in some experiments [6] with transient gross boiling in the annular gap small gas bubbles were introduced into the test section from below. Thereupon, only an attenuated and strongly modified boiling signal could be measured at the flowmeter above the heated section. This means that the sodium column is decoupled by the gas above the test section from the condensation and boiling zones, respectively, and does no longer reveal the characteristic pulsations. This result is significant in the assessment of boiling detection methods for the reactor since fission gases might appear there.

3. Future Test Projects

3. 1 Total Blockage and Pump Coastdown in a Multi-Rod Bundle

In 1974 experiments are to be conducted in a 7-rod bundle concerning pump coastdown and total blockage. The rods will have a heated length of 600 mm
with a specific rating of 170 W/cm². Specific importance will be attached to
a good simulation of the hydraulic conditions above the upper unfueled section
of the subassembly.

The objectives of this test series are mainly that listed under point 2.1 with
special emphasis on the bundle geometry. The topics can be summarized as
follows:

- Compare the results from the 7-pin bundle with those obtained in single
  channel geometries.
- Check the BLOW 3 code with the experimental results.
- Verify the possibility to extrapolate the results to the fuel elements of SNR.

A prototype of the 7-rod bundle was tested successfully under steady-state
boiling conditions, at $T_s = 950^\circ C$. In case major questions arising from the
test series with the 7-rod bundle cannot be answered, a second test series is
planned with a 37-rod bundle.

The investigations of the critical heat flux, the film thicknesses formed, etc.
are extended to other tube geometries. Also the effects of the hydrodynamic
instability will be included.

3.2 Local Blockages in the Multi-Rod Bundle

To investigate local blockages and local boiling events in a full scale rod bundle
two experimental projects are about to be realized. In cooperation with RCN
(Netherlands) bundles with 28 electrically heated rods are under construction
for blockages between 40 - 75\(^\circ\)/o of the flow section. They will be inserted
in the sodium boiling loop at RCN. These are 60\(^\circ\) bundle segments correspon-
ding to the SNR fuel element geometry. It is expected that the results will be
available in 1975.

At GfK a relatively large sodium boiling loop will start operation in 1974. The
flow corresponds to that of an SNR fuel element. In the initial phase, two
bundles with 169 rods each will be operated in this loop, 91 of them heated
electrically over a length of 300 mm. Both peripheral and central blockages
are planned. The objectives of these two experimental projects can be summa-
rized as follows:

- Investigate the nature and development of local boiling processes in bundle
  geometries.
- Compare the results with those obtained with the "negative bundle" test
  section and verify the theoretical model.
- Answer the questions concerning detection and propagation of local flow
  disturbances.

Moreover, investigations have to be mentioned concerning the detection of
boiling noise generated by an electrically heated boiling probe in the KNK-
reactor as well as in-pile experiments with individual rods performed in
Petten (Netherlands), and in multi-rod configurations in Mol 7 C (Belgium).
A participation in the French SCARABEE program is discussed. The latter 3
projects go beyond boiling and include fuel rod failures.
3.3 Outlook Concerning Research of Coolant Disturbances

As to the development of cooling disturbances the question of the incipient boiling superheat to be anticipated remains still to be answered. Right now no prediction can be made. The incidents, however, must be controllable both under superheat conditions and in the absence of it.

Concerning the residual film thickness under transient boiling conditions the interaction of the various mechanisms cannot yet be represented satisfactorily in physical models. Additional test results are required.

A great uncertainty consists with respect to the formation mechanism and type of local blockages. Although several growth mechanisms are assumed, they still wait for experimental confirmation. This field must largely be regarded as an unknown territory.

Although in this paper mainly the Karlsruhe work on coolant disturbances was reviewed, it should be pointed out that this work is and will be essentially influenced by the worldwide discussion of LMFBR safety and the related efforts.

References


Fig. 6: Liquid Gas Interface during Ejection

Fig. 7: Electrically Heated Pin Bundle (Wrapper withdrawn)

Fig. 8

Fig. 9

GFK IRE

Pump Coast Down Characteristic of Test Run No.16/17. Qualitative Comparison with SNR-300

GFK IRE

Theoretical Model Schematic Graph
Mechanism of Ejection (Schematic)

Fig. 10

Fig. 11

Fig. 12
EFFECT OF PARTIAL BLOCKAGES IN SIMULATED LMFBR FUEL ASSEMBLIES

M. H. Fontana  R. E. MacPherson
P. A. Gnadt  L. F. Parsley
T. S. Kress  J. L. Wantland

Oak Ridge National Laboratory
Oak Ridge, Tennessee 37830
Contract No. W-7405-eng-26

ABSTRACT

Experimental data on the effects of partial blockages in simulated LMFBR rod bundles are reviewed and the results presented. Experiments performed in the ORNL Fuel Failure Mockup (FFM) with 13- and 24-channel inlet blockages in 19-rod sodium-cooled electrically heated rod bundles indicate that excessive temperatures do not occur as a result of the blockages. Similar experiments with 6-channel non-heat-generating blockages in the heated zone of the rod bundle indicate local temperature increases of ~170°F at 100% flow and at powers of ~10 kW/ft. A simple representation of flow recirculation in the wake of the blockage indicates that a small recirculating flow significantly reduces temperatures in the wake zone. Calculations of temperatures within heat-generating blockages indicate that in order to obtain temperatures above the cladding limit more than one subchannel must be blocked over a length in excess of 0.5 in.

1. INTRODUCTION

Experimental data from the Oak Ridge National Laboratory (ORNL) Liquid-Metal Fast Breeder Reactor (LMFBR) Fuel Failure Mockup facility (FFM) were reviewed to evaluate possible effects of hypothetical partial blockages in Fast Flux Test Facility (FFTF) fuel subassemblies. The FFM is an engineering-scale sodium flow loop in which fuel rod assemblies are simulated with 19-rod bundles of electric cartridge heaters having heat flux and external configuration similar to FFTF fuel. Data were obtained with bundle inlet blockages of 13 and 24 channels and with heated-zone internal blockages of 6 channels.

Various analytical models were used to interpret the data. The ORRIBLE code was used to compute the flow and temperature distributions in the rod bundles downstream of the recirculating wake zone. ORRIBLE uses a simplified flow formulation and is computationally stable for cases where partial areas of the rod bundle are blocked, but it has no provisions for computing detailed flow distributions within the recirculating wake zone behind a blockage. Temperature distributions in the recirculating wake zone immediately behind the blockage were estimated using simple arbitrary representations of the wake flow with varying levels of recirculation velocity and blockage sizes. Temperatures within the blockage itself were calculated using HEATING, a three-dimensional heat conduction code. Heat conduction computations were made for heat-generating and non-heat-generating blockages with arbitrary shapes and boundary conditions.

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This paper is a review of partial results obtained from a continuing program; more definitive conclusions may be drawn after completion of the program.

2. BACKGROUND

2.1 Types of Blockages

Partial blockage of rod bundles may be caused by foreign materials lodging at the inlet to the bundle. Blockages could be caused by single pieces of debris or by buildup of small particulate matter. (A plate about the size of a dime would be required to produce a blockage of about 13 channels). To place this discussion in perspective it should be noted that, since the Fermi accident, inlet flow paths to fuel assemblies have been designed to prevent complete blockage of a subassembly by a single piece of debris. Presently envisioned inlet configurations would not allow entry of foreign material larger than about 0.25 in diameter. The sizes of these entrance flow passages are not likely to be increased in the future because the pressure losses in that region are already small compared with the pressure losses in the fuel rod region.

The smallest flow paths in the system are the fuel subchannels, and buildup of foreign matter in the entrance region of the fuel bundle could conceivably cause excessive blockage. The consequences of such a blockage would depend on whether it is concentrated locally or distributed over the entire frontal surface of the rod bundle. If several contiguous channels were blocked, the temperature of the fuel in the heated zone might be excessive. (Experimental data given later in this report indicate that inlet blockages of less than 24 contiguous subchannels are not likely to cause excessive temperatures in the FFTF fuel). However, properly designed fuel assembly inlets should cause evenly distributed flow to the upstream face of the fuel rods; if the debris follows the flow paths, its buildup would be evenly distributed. In this case flow would probably be reduced in all subchannels uniformly and gradually, possibly allowing the detection of low flow prior to excessive pin damage due to high temperatures, because a uniform reduction in flow would cause a detectable general increase in fuel assembly temperature rather than a hot spot.

Estimates made of the reduction of flow as a consequence of partial subassembly blockages show that it is necessary to block about 50% of the total flow area of a subassembly to cause a 5% reduction in flow. This is so because the increased pressure drop due to partial blockages is small compared with the pressure drop due to the rod bundle itself. Therefore, it is not likely that solid blockages covering a small fraction of flow area could be detected prior to fuel failure.

Blockage within the heated zone of the rod bundle could conceivably be caused by 1) lodging of debris within fuel channels; 2) broken wire-wrap spacers; 3) swelling of fuel pins due to defects, weak spots, overenriched pellets, or poor heat transfer (caused perhaps by gas release, debris hangup, or distorted pins and adjacent swelling); 4) fuel debris from failed pins; or 5) widespread pin swelling due to power-flow mismatch during a loss-of-flow incident. All the above items except item 5 are classed as potential accident initiators and are being investigated, particularly with respect to their potential for failure propagation. It should be noted that lodging of debris is not likely in fuel with spiral wire-wrap spacers. Fuel swelling would be minimized by proper oxygen control in the sodium. Also, irradiated cladding is expected to swell only slightly prior to failure due to internal pressure and high temperature, and therefore would not cause extensive blockage of the flow channels. Therefore, in some respects, these blockages are hypothetical because of the phenomena that make their existence in the core unlikely.
2.2 Flow in the Wake Zone Behind Blockages

Considerable information is available on flow separation caused by obstructions such as cylinders, disks, steps in a surface of a plate, and walls perpendicular to the flow path. An extensive review of flow separation is given by Chang. Some attempts have been made to apply some of this information to the analysis of flow in rod bundles, and limited experimental work has been done with water. Heat transfer in parts of the wake zone may be enhanced. This has been observed in water experiments in rod bundles for four-channel and for six- and for 24-channel planar blockages.

Kirsch and Schleisiek made the rather startling prediction that flow recirculation in the separated flow zone behind a blockage would safely remove the heat generated behind a blockage extending to more than 40% of the total full-scale SNR Na-2 reactor bundle flow area. They investigated this effect with sodium flowing in an obstructed annulus having one wall shaped to loosely simulate fuel rods. They also conducted experiments in rod bundles with water, using salt injected into the wake zone. They found that the length of the recirculation zone was relatively independent of velocity, as was the mass interchange between the fluid in the recirculation zone and the "free stream" fluid. In a more recent report Kirsch indicates that, based on water experiments, temperature increases for sodium cooled rod bundles in the SNR reactor were estimated for a blocked area/total flow area ratio of 0.147 and 0.411 for a central blockage and 0.411 for a blockage adjacent to the duct wall. For the 0.147 area ratio central blockage, sodium temperature in the recirculation zone of 232°C (418°F) higher than the coolant temperature immediately ahead of the blockage was predicted. For a central blockage of blocked/total flow area ratio of 0.411 (a blockage radius of 0.3.8 cm), the predicted temperature rise was 311°C (560°F). For an edge blockage of 0.411 area ratio, the temperature rise was 411°C (739°F).

Gast predicted that void growth and collapse would serve to remove the heat from the region behind a blockage of 55% of the bundle flow area (4.1 cm radius). He stated, "The analytical results lead to preliminary conclusions that, even at superheat as high as 150°C (270°F) individual vapor bubbles re-condense within 35 msec, that the fuel cladding within the bubble is not overheated during this time, and that local boiling is unlikely to cause thermal-hydraulic instability in the affected fuel subassembly. Furthermore, it is shown that in case of considerable superheat, the rapid volume change of individual vapor bubbles causes variations of the coolant flow rate at the subassembly outlet, which may be detected by means of an electromagnetic flowmeter." The calculations were performed on the assumption that the fuel rods had no effect on the hydrodynamics. This analysis must be checked experimentally and, if substantiated, should essentially solve the problem of subassembly blockage (particularly if constituted of non-heat-generating material) with respect to gross heat removal from the blocked zone.

Steifel performed experiments in two parallel channels, one of which was blocked. He found that pressure equalized rapidly downstream of the blockage, but differences in flow persisted. This observation might be of value in the development of detection techniques because the coolant in the channels having lower flow should leave the bundle at higher temperatures.

In practice, it is unlikely that a large blockage will occur that is completely impervious. A slight leakage through a blocked zone might significantly diminish temperature in the blocked region. However, Basmer, Kirsch, and Schultheiss show that slight leakage can destroy the recirculating flow patterns in the wake zone and possibly might hinder heat transfer in this region. The relative importance of these effects is not known at this time but will be investigated in the ORNL LMFBR-FFM program.
3. FLOW BLOCKAGE EXPERIMENTS IN THE FUEL FAILURE MOCKUP (FFM)

3.1 Description of the Facility

The FFM is a large-scale sodium flow facility at ORNL. It has a centrifugal pump with a flow capacity of 600 gpm, which is adequate for testing full-scale 217-rod subassemblies. However, at the present time the power controllers and the heat dump limit the power to 422 kW, which allows 24.5 kW per rod in a 19-rod bundle. Experiments are conducted with rod bundles in which fuel rods are simulated by electrical heaters having the same linear power density (up to 17.5 kW/ft) and external configuration as the FFTF fuel rods. The rods are 0.230 in. in diameter and are spaced by 0.056-in.-diam wires wrapped on a 12-in. pitch. Heater rods are described in reference 26.

The facility has provisions for subdivided control of the individual groups of heaters, so that many combinations of heaters can be operated at any power level up to 24.5 kW/ft each.

Temperatures throughout the rod bundle are measured (without causing flow perturbations) by thermocouples placed within the heater sheath and within the wire-wrap spacers. Descriptions of the instrumentation are given in later sections of this report. Detailed descriptions of the facility are given in refs. 27 and 28.

3.2 Effect of 13- and 24-Channel Inlet Blockage in FFM Bundle 2B

3.2.1 Description of Rod Bundle 2

FFM bundle 2 is a 19-rod bundle in a hexagonal duct. The dimensions and configuration of the rods and spacers are similar to those of the FFTF fuel subassembly except that the heated length is 21 in. rather than 36 in., and there is a 3-in. unheated length between the heated section and the free end of the bundle, rather than 6 in.

Originally, bundle 2 was not intended for inlet blockage investigations; it was designed for baseline thermal hydraulic investigations in unblocked condition. Most of the instrumentation was at the plane 18 in. above the start of the heated zone, which corresponds to the midplane of the FFTF core. The free end of the bundle was the downstream end, thus allowing the use of a thermocouple rake for measuring the exit sodium temperature profile. Results of these experiments are presented in refs. 29 and 30.

After the base line thermal-hydraulic test program was completed, the bundle was inverted in the test section so that the free ends of the heaters faced upstream, thereby allowing testing with inlet blockages. This orientation resulted in a 3-in. unheated entrance length followed by a 21-in. heated length. In this configuration, the heavily instrumented plane is 3 in. above the inlet to the heated zone (6 in. above the inlet face of the rod bundle). Thus, most of the instrumentation was well into the heated section but reasonably close (6 in. or less) to the inlet blockage. The orientation of the bundle in the test section for the inlet blockage tests is shown in Fig. 1. The bundle in its original orientation was designated FFM bundle 2A, and the inverted orientation for inlet blockage tests, it was designated FFM bundle 2B.

The four types of temperature instrumentation in this bundle are described below:

1. Thirteen wire-wrap spacers each contain two ungrounded Chromel-Alumel thermocouples spaced 2 or 12 in. apart axially.

2. Six wire-wrap spacers each contain two grounded Chromel-Alumel thermocouples diametrically opposed in the wrap; in bundle 2B three pairs are at the 2-in. level, and three pairs are at the 3-in. level.
Figure 1. Test section orientation for FFM bundle 2B.
3. Alternate Chromel and Alumel bare wires (10 mils in diameter) are installed in the heater in the 0.039-in. clearance between the heating element and the sheath. These wires are separately joined to the sheath to form an intrinsic thermocouple junction on the inner surface of the heater sheath.

4. Chromel-Alumel thermocouples are installed at intervals along the bundle length to measure the inner wall temperatures of the hexagonal duct.

The locations of the heater internal thermocouples, the grounded wire-wrap thermocouples, and the 3-in.-level duct-wall thermocouples for FFM bundle 2B are shown in Fig. 2, along with the rod and channel numbering convention.

The large circles represent the heaters that simulate the fuel rods. These are identified by the central number. The small tangent circles indicate thermocouple junctions at the indicated azimuthal position of the wire-wrap spacers. The junctions are located at an axial level indicated by the numbers in the small circles, which have units of inches from the start of the heated zone. The small circles containing pairs of dots indicate the location of grounded-junction thermocouples. The pair of dots next to the heater surface indicates that a thermocouple junction in the wire wrap is adjacent to the heater, whereas the pairs of dots on the opposite side indicates that the other junction, at the same axial level, measures temperatures near the center of the flow subchannel. The flow subchannels, defined by the lines connecting the centers of the heaters, are identified by the numbers in the triangles so defined. The fuel-rod-simulator heaters have thermal elements attached to the inner surface of the cladding as indicated by the dots labeled A, B, C, etc., in the large circles.

3.2.2 Summary of Results from Inlet Blockage Experiments

Testing was conducted with (1) no inlet blockage, (2) 13 channels blocked (channels 1 to 6 and 13 to 19; see Fig. 2), and (3) 24 channels blocked (channels 1 to 24 - all but the peripheral channels). With the 24-channel inlet blockage plate installed, approximately half of the net flow cross-sectional area was covered.

During this series of tests the flow was varied from 10 gpm (~20% of full flow) to 55 gpm (~100% of full flow) with all 19 rods heated at a uniform heat rate of 2 to 8 kW/ft per rod. Over this flow range the total bundle pressure drop did not increase significantly above the unblocked value with the addition of either inlet blockage plate.

The thermal results of these tests are summarized in Table I and Fig. 3. For comparison, the use of the dimensionless temperature rise \( (T - T_{in}) / (T_{out} - T_{in}) \) is convenient, where \( T \) is the temperature measured by the thermocouple under consideration, \( T_{in} \) is the sodium inlet temperature (~600°F), and \( T_{out} \) is the bulk sodium outlet temperature. Results from the duct wall thermocouples are not considered in this discussion, since they yielded little information with respect to blockages except that, as flow is diverted to the outer channels by centrally located blockages, relative wall temperatures are slightly depressed.

In these presentations all axial measurements are given in inches downstream from the start of the heated section, which is 3 in. downstream from the inlet blockage.

The dimensionless temperature rises from the wire-wrap thermocouples and from the heater internal thermocouples are given in Tables I and II, respectively, for no blockage, 13-channel inlet blockage, and 24-channel inlet blockage for all 19 rods heated at 4, 5, and 6 kW/ft per rod and a constant flow of 55 gpm. In this range, the effect of power level on the dimensionless temperature rise is small.

The ratios of \( (T - T_{in}) / (T_{out} - T_{in}) \) (blocked) to \( (T - T_{in}) / (T_{out} - T_{in}) \) (unblocked) for a flow of 55 gpm with all 19 rods heated at 5 kW/ft per rod are given in Fig. 3 for the 24-channel inlet blockage. (Space limitation prevents the presentation of the results for the case of 13 channels blocked. The temperature rises due to it are considerably smaller). It may be seen from this figure that, in general, this ratio is greater than one for channels downstream...
Figure 2. LMFBR-FFM bundle No. 2B instrumentation.
Table I. Comparison of dimensionless temperature rises [(T - Tin)/(Tout - Tin)] measured by the wire-wrap thermocouples in FFM bundle 2B with all 19 rods heated, 55 gpm flow, and Tin 600°F

<table>
<thead>
<tr>
<th>Rod No.</th>
<th>Axial plane (in.)</th>
<th>Channel No.</th>
<th>4 kW/ft (Tout - Tin = 61°F)</th>
<th>5 kW/ft (Tout - Tin = 76°F)</th>
<th>6 kW/ft (Tout - Tin = 91°F)</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td></td>
<td>No blockage</td>
<td>13-channel blockage</td>
<td>24-channel blockage</td>
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<tr>
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<tr>
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Ungrounded thermocouples

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</table>
Table II. Comparison of dimensionless temperature rises \((T - T_{in})/(T_{out} - T_{in})\) measured by the heater internal thermocouples in FFM bundle 2B with all 19 rods heated, 55 gpm flow, and \(T_{in} \sim 600°F\)

<table>
<thead>
<tr>
<th>Rod No.</th>
<th>Axial plane (in.)</th>
<th>Location</th>
<th>4 kW/ft ((T_{out} - T_{in} = 61°F))</th>
<th>5 kW/ft ((T_{out} - T_{in} = 76°F))</th>
<th>6 kW/ft ((T_{out} - T_{in} = 91°F))</th>
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<tr>
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<td>13-channel blockage</td>
<td>24-channel blockage</td>
</tr>
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<td>1.01</td>
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</tr>
<tr>
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<td>1.14</td>
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</tr>
<tr>
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</table>

\(^{a}\)See Fig. 4 for identification.
\(^{b}\)Deleted, standard deviation \(>10\).
\(^{c}\)Suspected of being in error.
Figure 3. Ratio of dimensionless temperature rises above inlet vs axial position for FFM bundle 2B with blockage of 24 channels (1-24) at the inlet 55 gpm flow, ~600°F inlet temperature. Averages of blockage runs 738-742 and 747 compared with averages of unblocked runs 700-702.

of the inlet blockage and, due to the increased bypass flow, is less than one for the unblocked channels (channels 24 to 42 for the 24-channel inlet blockage). It may be seen that the effect of blockage, as indicated by substantial departures of the ratio from unity, is limited to about 3 in. downstream of the start of the heated section (6 in. from the inlet blockage plate) or about five to six equivalent blockage diameters downstream. No excessively high temperatures were observed. The highest temperature ratio occurred for a 24-channel blockage (55 gpm flow) in channel 22 at an axial position 2 in. downstream from the start of the heated section (5 in. from the inlet blockage). The ratio at that location was ~1.8, indicating an 80% increase in the temperature rise over that of the unblocked case. Since the temperature rise for the unblocked case at that position was ~7°F and the temperature rise in the blocked case was ~13°F, the blockage caused an increase of only ~6°F. The higher dimensionless temperature ratios shown on Fig. 3 for the thermocouples at the start of the heated zone indicate that some flow recirculation behind the blockage was extending to the heated zone. The small amount of backward flow from the heated zone to the position of these thermocouples would heat them greater than would be expected if the flow were not recirculating.
3.2.3 Comments on Inlet Blockage Experiment

It is concluded that centrally located inlet blockages of up to one half of the flow area of 19-rod bundle with a 3-in. unheated entrance length do not result in excessively high temperatures. The temperature increases attributed to the inlet blockages are of the same order as the temperature variations normally observed in unblocked bundles.

Since the unheated entrance length of the fuel pins in an FFTF 217-pin subassembly is 6 in., twice that of the heaters used in the tests reported here, the flow maldistribution caused by the inlet blockage should be further ameliorated prior to reaching the heated zone of the reactor. Therefore, one would expect lower temperature increases in the FFTF subassembly than are indicated by these results. However, there are two other differences between the 19-rod experiment and the FFTF whose effects are difficult to extrapolate to the FFTF configuration. In these experiments the fractions of the frontal flow area covered by the inlet blockage plates were quite large; for example the 24-channel inlet blockage covered 50% of the total flow area. Because of this the fluid velocities around the blockage plate were higher than nominal, and these higher velocities may have aided in correcting the flow maldistributions caused by the blockages. Also, the proximity of the duct wall to the blockage may have had some influence in diverting the flow inward behind the blockages as compared with the relative remoteness of the wall in an FFTF subassembly. These two effects (which probably interact) would cause these tests to underpredict local temperature rises caused by similarly sized inlet blockages in larger bundles. However, it is doubtful that these effects will be sufficient to offset the mitigating effect of the longer unheated entrance length of the full-size fuel assembly. On the basis of the small temperature increases observed in these tests, it is concluded that inlet blockages of as many as 24 contiguous subchannels will not result in excessively high temperatures in the FFTF 217-pin subassembly.

3.3 Effect of Internal Blockage in FFM Bundle 3A

3.3.1 Description of Rod Bundle 3A

Bundle 3A of the FFM program was also of FFTF configuration. Like bundle 2B, bundle 3A had 19 rods of 21 in. heated length. However, instead of a hexagonal duct the duct in bundle 3A was round, with dummy rods having wire wrap spacers inserted in the duct wall to simulate an infinite array. Its central six channels were blocked by a non-heat-generating stainless steel device, 1/4 in. long, brazed to the central rod at the elevation 15 in from the start of the heated zone.

In this series of experiments the bundle was inserted from the bottom of the test section with the free ends of the heaters facing upward. This allowed the use of a thermocouple rake, entering from the opposite end of the test section, for monitoring exit temperatures from selected flow channels.

The bundle instrumentation layout is shown in Fig. 4. The convention for identifying thermocouples, heaters, and subchannels is similar to that of bundle 2B, described previously, except that the positions of the exit rake thermocouples are indicated by circles with crosses in them.

In bundle 3A, the ends of these thermal elements internal to the heaters were grounded to the inner surface of the cladding at 15° azimuthal intervals and at 0.25 in. axial intervals; thus the junction formed by two thermal elements measured an average temperature along the spiral path on the inner surface of cladding between the two junctions. Notice that the thermal elements in heaters 1, 2, and 3 measure temperatures from 15 to 16 in. (from the start of the heated zone) in 1/4-in. increments; those in heater 6 measure from 15.22 to 16.22 in.; and those in heaters 4, 5, and 7 measure from 16 to 17 in.
Figure 4. Spacer wire and internal thermocouple locations for FFM bundle 3A.
3.3.2 Summary of Results from Heated-Zone Blockage Experiments

Five sets of experiments have been performed with bundle 3A. These included cases with no power; all rods heated at powers of 5, 7.5, and 10 kW/ft with flows of 54 gpm (100% of full flow) and 11 gpm (20% of full flow); central seven rods heated at 10 kW/ft with a flow of 54 gpm; and three outer rods heated at each "flat" of the hexagon defined by the outer row of rods at a flow of 54 gpm. These latter tests are described in ref. 31. Because of space limitations, this discussion is restricted to the experiments performed at 100% flow and 10 kW/ft. The results are described more fully in references 19 and 32.

Figure 5 shows the central subchannel temperatures, presented as $T - T_{\text{inlet}}$ vs distance from the start of the heated zone for run 101 (10 kW/ft, 100% flow). This run is of particular interest because it represents the case of 100% of FFTF specific flow (54 gpm for 19 rods) and a power of 10 kW/ft, which is significantly above the average FFTF linear power density. In these experiments, temperatures were measured by thermocouples inside the central seven heaters and by the wire-wrap thermocouples in the central six subchannels.

The temperatures measured by the thermocouples inside the heaters are shown in Fig. 5 as horizontal lines extending the axial distance between the two thermal elements that make up the particular thermocouple being plotted. This distance is usually 1/4 in., and the indicated temperature may be considered as an average along that length. The number near each line indicates the rod within which that particular thermocouple resides. The outer cladding surface temperature was computed by subtracting a temperature drop across the cladding as calculated for the given heat flux assuming radial heat flux (the latter assumption should be valid everywhere except directly underneath the blockage device). These computed outer cladding surface temperatures are indicated in the figures by the subscript c.

Temperatures measured by thermocouples in the wire-wrap spacers are plotted and labeled so that the first two digits indicate the heater to which the wire-wrap spacer is attached, the second two digits indicate the axial distance of the thermocouple junction downstream from the start of the heated zone, and the last digit indicates the channel in which the spacer resides at that particular axial elevation. For example 04 13 CH 3 indicates that the thermocouple junction is in heater 4, 13 in. from start of heated zone, facing channel 3. Grounded-junction thermocouples in the wire-wrap spacers indicate two temperatures at the same elevation, one near the heater surface and the other near the center of the flow subchannel. These are also plotted.

The 90% (2σ) confidence limit bands (based on repeated measurements) are not shown because of their small and relatively constant values. Except for internal thermocouples 0115AB, which had a standard deviation, $\sigma$, of 3.1°F for the worst case, and 0115DE, for which $\sigma$ was 3.7°F, all other internal thermocouples had standard deviations of about 0.3 to 1°F. Those of the wire-wrap thermocouples were about 0.5°F.

The abscissa in Fig. 5 starts at 0.3 in. from the start of the heated zone because all information of interest is downstream of this point. The blockage plate is shown at the 15-in. level. The estimated length of the recirculation zone is shown as 2 in., which is about seven times the radius of the blockage disk; or 12 times the step height of the blockage device above the surface of the central rod. Several investigators have indicated that the recirculation zone for flow over an obstruction is 5.2 to 17 times the radius of a disc or the height of the step.17,99 The first value is for flow over a sharp edge disc in a free stream, and the second is for flow over a sharp edge wall on a flat plate. Other configurations have intermediate values.

Figure 5 also shows bulk mixed-mean temperature rises calculated by heat balances. In the region of the blockage the highest measured temperature (rod 7 at 16 to 16.25 in.), adjusted to give the external cladding temperature, is approximately 220°F higher than the mixed-mean temperature at that point. However, it is not appropriate to use the mixed-mean temperature as a base for comparison because temperature peaking would also exist in the center of un-blocked rod bundles.
Figure 5. Temperatures along the central six channels, bundle 3A, run 101. 100% flow, 10 kW/ft.
The temperatures for channel 3 of unblocked bundle 2A, run 1109 (54 gpm, 10 kW/ft), as calculated using the ORRIBLE code were compared with some pertinent experimental data points. The code parameters used were $C_T = 0.005$, $C_D = 0.6$, and $C_S = 1.0$, where $C_T$ is a parameter related to the turbulent mixing between subchannels, $C_D$ is related to the pressure diversion cross flow, and $C_S$ is related to the sweeping of fluid by the spiral wire-wrap spacers. See ref. 1 for a definition of these parameters. (Calculated temperatures are most sensitive to $C_S$ and are relatively insensitive to the other two coefficients). The good agreement between computed and experimental temperatures in bundle 2A indicated that a computed temperature distribution for bundle 3A, in the unblocked configuration, should serve as a reasonably good reference for comparison of the results from blocked bundle 3A.

The calculated axial temperature distribution of channel 3 (if it were unblocked) is shown on the second line from the top in Fig. 5. The cladding outer surface temperatures appear to be about 80 to $\Delta 180^\circ$F above the average temperature of the unblocked channel at the 15 to 17-in. level. Figure 5 also shows the temperatures measured by the ungrounded thermocouples in the wire-wrap spacers in the central six subchannels: 04 13 CH 3, 01 15 CH 4, 03 15 CH 2, 01 17 CH 3, 06 19 CH 4, 06 21 CH 5, and 05 21 CH 3. Also plotted are the measurements from the grounded-junction thermocouples, which show the radial temperature difference across the wire-wrap spacers: 02 15 CH 1 and 07 19 CH 6. These indicate that the $\Delta T$s across the wire wraps are about 15°F (02 17 CH 1) to 25°F (07 19 CH 6). Since the ungrounded thermocouple junctions are approximately in the center of the wire-wrap spacers, a rough estimate can be made of the sodium channel temperatures by subtracting half the $\Delta T$ obtained from the grounded-junction thermocouples (7 to 12°F) from the readings obtained with the ungrounded-junction thermocouples. Adjusted readings (plotted in Fig. 5) are compared with ORRIBLE predictions for temperatures in channel 3 (which for purposes of presentation, serves as an indicator of the behavior of all six central channels) in the blocked configuration. Apparently, the prediction of temperatures downstream from the blockage is reasonably good for our purposes. Exit temperatures are also plotted and discussed more fully later.

Since ORRIBLE has no provisions for calculating recirculating flow, predictions obtained with it should not be valid in the recirculating zone. If it is assumed that a 10 to 20°F "film drop $\Delta T$" exists between the cladding outer surface and the average channel sodium temperature, the temperatures of the sodium in the recirculating zone could be estimated as being in the range enclosed by the two dashed lines in Fig. 5.

It is interesting to note the factors that could affect some of the measurements plotted in Fig. 5. Referring to Fig. 4, the thermal elements in rod 7 are located from 1 in. upstream from the six o'clock (17 in.) position of the associated wire wrap to 2 in. upstream of the four o'clock position of the wire wrap. At the 16-in. level, the wire wrap on rod 7 creates a dam between rods 6 and 7 and could "trap" hot fluid below this zone, thus possibly accounting for the higher temperatures indicated by rod 7 in Fig. 5.

The spiral formed by the wire-wrap spacer on rod 1 touches rod 5 at a point 0.5 in. below the spiral defined by the rod 5 thermal elements. The closeness of the rod 1 spacer might be affecting the temperatures in rod 5.

The thermal elements in rods 1, 3, 4, and 6 appear to be relatively free of the influence of wire wraps. Those on heater 1 face channel 1, whereas the others face adjacent rods.

The low temperatures in heater 2 are somewhat puzzling. One possible explanation is that the wire wrap, which penetrates the blockage plate at the eight o'clock position, might be leaking sufficient fluid to depress the local temperatures.

All these second-order effects do not significantly affect the main conclusion that can be drawn from these results: a blockage of the shape tested is tolerable at full flow and power.
Space limitations prevent discussion of detailed data from experiments at lower flows. Some high points of the experiment at 10 kW/ft and 60% flow follow. For this case the cladding outer surface temperatures in the vicinity of the blockage ranged from 80 to 220°F higher than the sodium temperature in the central channels (represented by channel 3) expected for the unblocked case. The hottest cladding internal surface temperature was 132°F, which is about 540°F higher than the inlet temperature of 78°F. It should be noted that the temperature increase caused by the blockage is only slightly affected by flow. These results indicate that a non-heat-generating blockage of the size tested is acceptable even at 60% flow conditions.

The temperatures at the exits of selected subchannels, as indicated by crossed circles in Fig. 4, were measured using the exit thermocouple rake. These indicated temperatures should represent the particular subchannel exit mixed-mean temperature because of the mixing that occurs in the 3 in. of unheated length between the end of the heated zone and the channel exit. Thus, these measurements provide results that can be directly compared with analytical predictions of mixed-mean subchannel temperatures. By comparison with the results from unblocked bundles, the magnitude of the influence of an in-core blockage on the exit temperature profile can be determined. This should help indicate the feasibility of detecting the blockage by thermal devices located in the exit region.

To provide an exit temperature profile from an unblocked case for comparison, a diametral traverse of the normalized temperature distribution \((T - T_{in})/(T_{out} - T_{in})\) measured in unblocked bundle 2A (10 kW/ft; 53 gpm) were compared with ORRIBLE predictions for that case. Bundle 2A was identical to bundle 3A except that it was unblocked and had a hexagonal duct instead of a duct containing wire-wrap dummy rods. The agreement between experimental results and analysis (within \(\pm 6^\circ F\)) indicates that ORRIBLE is a good predictor for that case. The agreement between the calculated and the measured bundle 2A temperature distribution indicates that the calculation for the unblocked bundle 3A may serve as a reference for comparison with the results of the blocked bundle experiments.

Figure 6 shows the calculated temperature distribution across the exit for the unblocked case and the calculated and experimentally determined temperature for the blocked case of 10 kW/ft, 100% flow (54 gpm). The pertinent experimental results for bundle 3A plotted in Fig. 6 show a temperature increase in the channels downstream from the blocked region (over the unblocked case) of \(\sim 30^\circ F\). The ORRIBLE predictions for the blocked case show a better agreement at channel 6 (within \(\sim 16^\circ F\)) and poorer agreement in the exterior channels. The poorer agreement in the exterior channels may be due to steeper, unequilibrated temperature gradients in that region. The ORRIBLE code calculates average sub-channel temperatures, whereas the thermocouples might be in a subchannel temperature gradient. Investigations of this effect are continuing. At any rate, these variations in the edge rows should not have a major effect on the center of the rod bundle.

The FFF fuel has exit gas plenums of 42 in. Experiments with full-length bundles are planned, and exit temperature distributions obtained from those experiments may indicate the feasibility of blockage detection under more realistic conditions.

3.3.3 Comments on Internal Blockage Experiments

Excessive temperatures are not generated in the heater rods as a consequence of a 0.25-in.-long, non-heat-generating blockage over an area of six subchannels in the 19-pin FFM bundle 3A even at 10 kW/ft and 60% flow. The blockage covers a flow area of only about 10% of the total area. Therefore, one would expect the wall effects on the flow in the vicinity of the blockage to be small. A non-heat-generating blockage of the small size would be expected to behave essentially the same way in a full-size 217-pin FFF fuel subassembly and therefore would not cause excessive temperatures.
4. ANALYSES OF BLOCKAGE EFFECTS

4.1 Analytical Representation of Flow Downstream from Planar Blockages

A model is being developed in which the flow recirculation zone is treated as a cylinder having dimensions similar to those measured for real wakes. The zone is divided into concentric cylindrical subzones with fluid flowing in the direction of the free stream flow in the peripheral flow subzones and in the opposite direction in the central subzones. The cells contain no surfaces or subchannels that correspond to fuel rods; this difference being rationalized by evidence that recirculation zones in blocked bundles may resemble wakes in free stream flow. For large blockages, heat is added as a volume heat generation term. For small blockages (for example, one ring of flow channels) heat generation on the surfaces of the recirculation zone is simulated.

Preliminary comparisons with salt injection experiments in a water mockup of the FFM, and with a six-channel blockage in the sodium-cooled FFM, indicate that agreement can be reached by choosing reasonable values for the recirculation velocities (e.g. about one tenth of the free stream velocity, in the peripheral flow zone).

The model indicates that the peak temperatures within the recirculation zone are most sensitive to the blockage diameter and internal recirculation velocity. It also indicates that for blockage in excess of six subchannels, molecular conduction alone (i.e. stagnant fluid) is not adequate to prevent excessive cladding temperatures.
The model is not sufficiently developed to warrant further discussion at this time. It is mentioned because it appears that the study of artificial recirculation cells may be a convenient means for gaining insight to the blockage problems and may serve as an intermediate step toward analyzing blockages until more realistic models become available.

4.2 Temperature Distributions within Blockages

In order to obtain estimates of the temperature distributions in in-core blockages, some simple cases have been analyzed using the HEATING computer code. The cases examined were (1) infinitely long blockages of one, two, and three adjacent subchannels adjoining a single rod; (2) thin planar blockages of infinite radius (0.1 and 0.25 in. thick); and (3) thin planar blockage of a radius equivalent to a six-channel blockage. Blockages composed of both heat-generating material (UO$_2$) and non-heat-generating material (stainless steel) have been investigated.

Calculations were made for the case of a single channel blocked by a heat-generating material of infinite length. (Lengths greater than 0.5 in. are essentially infinite with respect to heat flow paths). The temperatures are predicted to reach a maximum value of less than 2500°F at the center of the fuel and 1850°F in the center of the blocked channel and therefore are acceptable.

The results calculated for the case of blockage by a heat generating material of two adjoining channels show that temperatures at the center of the blockage region will approach the melting point of UO$_2$. It appears that the direction of heat flow is into the fuel pins and then out into the unblocked region. This configuration is not acceptable.

Calculations were performed to obtain the temperatures at the center planes of non-heat-generating planar blockages 0.10 and 0.25 in. thick having infinite radius. A thickness of 0.25 in. is near the maximum allowable thickness of a stainless steel blockage whose radius is too large for significant radial heat conduction.

Calculated temperatures in the center plane of a 0.25-in.-thick heat-generating blockage of infinite radius, without radial conduction, will be excessive.

The temperature distribution was calculated for the case of a heat-generating planar blockage ring, 0.25 in. long and 0.20 in. in radius, where the outer surface of the blockage transfers heat to the sodium at 1200°F. For this case the cladding temperature would be about 2500°F. Even if this type of blockage should accumulate in a zone where the temperature is 800°F, the cladding temperature would be above 2100°F and likely would fail. If the heat-generating ring blockage is reduced to 0.125 in. thickness instead of 0.250, its presence would be acceptable.

6. CONCLUSIONS

Blockages at the inlet of the fuel rod bundles caused by a single piece of material small enough to pass through the FFTF inlet flow paths will have little effect on temperatures internal to the fuel subassembly. Experiments were performed with 19-rod bundles in the FFM having identical configuration and heat flux as the FFTF with solid blockages of 13 and 24 channels at the inlet of the heated zones. The effects of 13-channel blockages were barely discernible. The internal temperatures were elevated as a consequence of 24-channel blockages, but they did not exceed the exit temperature for a normal unblocked subassembly.

The effects on the 24-channel blockage results of the nearness of the channel walls in the 19-pin experimental assembly and the manner in which it relates to the full-size FFTF 217-pin subassembly are not known. On the other hand, the FFTF has a 6-in. unheated inlet zone, and the longer length would allow more mixing and reestablishment of flow before entering the heated zone. From this standpoint the experimental results are conservative.
If an inlet blockage can be caused by a gradual buildup of many small pieces, it may be possible, but unlikely, to generate a solid blockage of continually greater size. The critical size of the blockage (where the blockage is either intolerable or detectable) will be determined in future investigations.

It has been experimentally established that an internal blockage of six subchannels of non-heat-generating material (stainless steel) 0.25 in. long is tolerable but is not detectable with present FFTF subchannel instrumentation. However, the exit temperature distribution is distorted sufficiently that detection might be possible in future reactors with multiple exit thermocouple correlation techniques.

Calculations indicate that a long (>0.5 in) heat-generating blockage of a single subchannel is tolerable, but a blockage of two adjacent channels is not. Also, a ring blockage of stainless steel 0.25 in. thick and of infinite radius (no radial heat loss) is barely tolerable. A ring blockage of heat-generating material of 0.20 in. radius must be less than 0.125 in. long to be acceptable.

Analysis of simple representations of flow in a recirculation zone indicates that the internal fluid temperatures will depend strongly on blockage size and recirculation velocities.

In the near future, the FFM program will be directed toward determining maximum permissible blockage size (of various shapes and leakages) coupled with the development of detectability techniques. The eventual desired result is that plant safety philosophy be freed from considerings of "credibility" and be based on detection prior to the attainment of some known damage limit. Experimental confirmation of maximum allowable internal blockage size is not yet available. However, present experimental results for all blockages tested indicate that blockages of these sizes will not create excessive temperatures in the bundle assembly.
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Studies of the Development of Coolant Boiling in a Fast Reactor

A. J. Clare, S. J. Board, R. S. Hall

Central Electricity Generating Board Berkeley Nuclear Laboratories Berkeley Gloucestershire England

Abstract

Information obtained about the onset and development of coolant boiling in a fast reactor is of considerable importance in determining its ultimate safety consequences, and is also essential as an aid to quantifying the detectable signals which may be generated by the processes. Experiments in a water rig are described which aim to define the temperature distribution around blockages in a pin-cluster, and the consequent coolant boiling patterns. Analysis of the data has shown the effects of the interaction of the vapour bubbles with the flow and temperature fields. The requirements of a coolant boiling model are discussed, including the effect of perturbations such as the release of gas from failed fuel pins. The implications of the programme of work on reactor safety, and fault detection, are discussed.
1. Introduction

It is generally accepted that one of the most difficult problem areas in Fast Reactor Safety concerns accidents originating from defects within a single sub-assembly. It is not possible at present to demonstrate rigorously that a sub-assembly accident will not be so large as to affect the whole core. Even if this demonstration should become possible in the future, the fault would still be of great significance with regard to plant availability, because of the need to remove both the damaged sub-assembly from the core and any released fuel debris from the primary circuit.

There is therefore a clear need to obtain a good understanding of the processes which may take place during the development of a significant sub-assembly defect, both to quantify the rate at which the fault may progress, and also to assist with the definition of the detection instrument specification.

There are two main schools of thought about the way in which a local defect within the sub-assembly could develop such that it could potentially endanger the whole core. The first is that a blockage would have to develop to a sufficient size for sodium boiling to occur downstream of the blockage and that either dryout would occur during boiling, or flow restrictions due to the vapour produced would permit the boiling to propagate to a large fraction of the sub-assembly, either route leading to fuel melting. The alternative view is that the release of fission-product gas from a failed fuel pin may cause gas blanketting of other pins, leading to pin-to-pin failure propagation, and resulting in loss of heat transfer from the fuel to the coolant and consequent fuel melting.

The present paper includes consideration of a combination of these two views, in that it discusses the pre-boiling temperature distribution behind a blockage and the subsequent boiling patterns, but it also considers the possible effects of the release of fission-product gases on the latter. The paper concludes with a review of the implications of the work on the detection thresholds which would be required to ensure that a sub-assembly accident could not involve sufficient molten fuel to endanger the whole core.

2. Coolant temperature at a blockage in a pin-cluster

2.1 An understanding of the temperature distribution in the coolant in the vicinity of a blockage is important for the prediction of the conditions the fuel will experience, the size of blockage at which local boiling will occur, and in determining the initial boiling behaviour.

Immediately downstream of the blockage is a region of turbulent, recirculating flow\(^1\). Calculation of velocity and temperature distributions in this region is made difficult even for a simple form of blockage by the pin cluster geometry, and by the present inadequate knowledge of turbulence in these conditions, so that considerable approximations are necessary, and experimental verification is essential. The most promising calculation of velocity and temperature distributions in the wake at present is the code SABRE\(^2\) in which the subchannel geometry is represented and which can treat reverse flow. The experimental work relating to coolant temperatures in the wake has been reviewed by Gregory and Lord\(^3\). The residence time of fluid in the wake has been measured by Kirsch and Schleisiek\(^4\) for water and sodium in a 'negative-bundle' geometry and for water in an SNR fuel assembly mock-up, and similar measurements have been made in a large scale water rig representing a 60° sector of the central part of a PFR sub-assembly\(^5\). Recent temperature measurements by Kirsch\(^6\) in a water cooled SNR fuel assembly mock-up suggest that at these
reactor conditions the sodium saturation temperature could be reached in the
wake of a blockage occupying about 50% of the flow area and situated against
a wall.

2.2 At Berkeley Nuclear Laboratories a small water rig has been used
for experiments in which coolant temperatures and some features of coolant
boiling at a blockage have been studied. Figure 1 shows the relevant details
of the test section, which includes a 24 mm diameter blockage surrounding 12
electrically heated pins, situated 12.5 mm up the heated section, there being
a further 12 unheated pins and 8 part-pins around the outside of the blockage.
The temperature distribution was measured using traversing thermocouples, the
result being shown in figure 2. In the figures the temperatures are normal-
ised (see Appendix I) to allow comparison between experiments at different
conditions of power and flow. It was found that the temperature at any
position was inversely proportional to the coolant flow-rate, over the range
of flow-rates used (70-220 cm/sec), so that as expected from dimensional
arguments the shape of the temperature field was independent of flow.

2.3 In order to aid understanding of the temperature distribution a
simple model of heat transport in a recirculating wake was developed. The
equation describing convection and turbulent and molecular diffusion of heat
was solved for a given velocity distribution; in the present case the distri-
bution used (see figure 3) was that measured in the wake of a disc in a fluid
stream. The pin cluster was not explicitly represented either in the heat
source distribution (which was represented as a uniform energy density input),
or in the velocity distribution, and uniform turbulence intensity was assumed.
The blockage diameter was the same as that used for the measurements and
conduction of heat in the blockage was allowed. The value of the eddy
diffusivity was varied over a wide range since it is the main unknown quantity.
Figure 3 shows the temperature distribution obtained normalised in the same
way as the experimental results: the peak temperature occurs at the downstream
face of the blockage, and is strongly dependent on the assumed conductivity of
the blockage material. However, the wake mean temperature is not so depend-
ent, as can be seen from figure 4(a). Figure 4(b) shows that both the mean
and the peak wake temperatures are not very sensitive to the degree of turbu-
ent heat transport over the relevant range of uncertainty (total diffusivity
= 0.1 - 1.0 cm²sec⁻¹). It is also evident in the model that the larger
thermal conductivity of sodium (0.64 cm²sec⁻¹) compared with that of water
(1.4 x 10⁻³ cm²sec⁻¹) makes a negligible difference to the heat transport at
the relevant velocities due to the dominant effect of eddy diffusivity (~ 1 cm²
sec⁻¹), so that sodium temperature distributions may be derived straightfor-
wardly from measured water temperature distributions (for positions not close to
pin walls).

Comparison between theory and experiment shows that the predicted mean
temperature is rather higher than observed, and it was found that the discre-
pancy could not be accounted for by representing the experimental unheated
outer zone in the model. There is, however, general agreement in the shape
of the distribution except near the axis; further work is required to resolve
the discrepancy. Because of the empirical velocity input the model is not
a predictive one, but it has proved of considerable value in gaining an
understanding of the relative importance of the various processes involved
in determining the wake temperature distribution.

Extrapolation to reactor conditions has been made to date largely on the
basis of measurements of wake residence times in a variety of experimental
scales. The information from these, treated empirically, is not sufficient
to confidently establish the scaling predictions for the independent variables
of blockage size, pipe size and hydraulic diameter. There are also insuffi-
cient actual temperature measurements to cover the necessary range for the
reactor case. A rig is under construction having a larger number (91) of heated pins surrounded by further unheated pins in order, inter alia, to check the more complex calculational methods.

2.4 A reactor sub-assembly blockage, if it is an accretion of particulate debris, is perhaps more likely to be permeable than impermeable. Residual flows through the blockage will affect coolant temperatures both by enhancing heat transfer in the blockage and by disturbing the downstream flow pattern.

Some indication of these effects has been obtained by performing an experiment using a blockage with perforations. These allow flow through the blockage in the form of an array of jets rather than a low velocity seepage, and the resulting temperature field (figure 5) clearly indicates the disturbing effects of the jets on the wake. Residual flow in this form greatly increases the turbulence in the wake, as well as modifying the mean flow pattern, and produces lower mean temperatures.

Theoretical work has suggested that a uniformly permeable blockage with a residual flow of less than about 20% would lead to higher wake temperatures than with an impermeable blockage, possibly by reducing the recirculating velocities and hence the turbulence. The measured temperature field produced by such a blockage with 10% residual flow is given in figure 6. It indicates a less disturbed wake than for a perforated blockage with comparable residual flow, and the peak temperature is about the same as for an impermeable blockage but at a position further downstream.

3. Local boiling at a blockage

3.1 Experiments on local boiling at a blockage by Lafay in a 19-pin water rig and by Schleisiek in a 'negative bundle' geometry with sodium have shown the pulsed form of boiling with growth and collapse of single vapour bubbles. Local boiling models by Gast, Brook and Fauske attempt to describe the growth and collapse of the first bubble but necessarily contain simplifications and do not properly consider potential reactor situations, such as the effect of the flow pressure field or fission product gas release from failed fuel pins, which might be expected to substantially extend the bubble lifetimes.

3.2 Preliminary experiments in the apparatus referred to above have shown that at reduced pressure (~0.5 bar), local boiling at a blockage in degassed water has the single vapour bubble growth and collapse behaviour which is expected to be characteristic of sodium boiling. Because of differences in thermal conductivity and sub-cooling it is not possible to correctly model in water the evaporation and condensation rates appropriate to sodium, but nevertheless the experiment is useful in studying some features of local boiling (such as the hydrodynamic behaviour) in conditions of turbulent flow and pin-cluster geometry.

Boiling at low superheat is produced by reducing the flow until boiling occurs in the region downstream of the blockage; typical conditions are 0.5 bar pressure and 100 cm.sec\(^{-1}\) mean coolant velocity in the test section. A stable cycle of growth and collapse of single bubbles is observed with a lifetime of ~120 ms and with a period typically of about 0.8s.

Cine film and thermocouple records taken during boiling give important information about the effect of flow forces on bubble dynamics, and about the distribution of displaced hot fluid around the growing bubble. Initially the bubble grows spherically until its diameter is equal to that of the blockage. The steep pressure and velocity gradients at the edge of the wake region...
cause further bubble growth to be predominantly axial at constant diameter and
at a velocity approximately equal to the bulk flow because at low superheat
the bubble driving pressure is not much greater than the $\frac{1}{2} \rho v^2$ forces; however,
when the top end of the bubble grows past the end of the normal recirculating
wake, the vapour also expands radially outwards into the main flow. The bubble
may divide into two at the end of the normal wake, the top portion drifting
upwards at the bulk flow velocity, and in this experiment, collapsing rapidly
as it engulfs cold pins. The lower portion remains attached to the blockage,
and, under the present experimental conditions, usually grows a little further,
and then collapses.

Transient temperatures recorded during this process indicate that at the
side of the bubble displaced hot liquid is swept away and the bubble appears
to be exposed to the bulk sub-cooling; however, a layer of hot liquid remains
around the top portion of the bubble.

After collapse of the bubble the wake region is occupied by sub-cooled
liquid and the wake is re-established. The recirculating region is then
heated to boiling conditions in a time dependent on the wake residence time.

3.3 The effects of adding gas

The effect of injecting non-condensible gas into the boiling wake has
been examined in order to assess the possibility that fission product gas
released from failed fuel at a sub-assembly blockage may perturb an other-
wise steady local boiling situation. It was found that the injection of
even small quantities of gas (about 2% of bubble volume) had the effect in
water of eliminating the waiting time between bubbles, but little effect
on the behaviour or life-time of the bubbles. From the cine films it was
clear that the waiting time was reduced because a small residual gas void
remained in the wake, and this regrew immediately because of vapour production
from the liquid film on the pins within it, despite the sub-cooling of the
surrounding liquid.

Whilst, in the reactor case, the much greater sub-cooling and thermal
conductivity of sodium would make immediate regrowth unlikely, the trapping
of the gas void in the wake would appear to be relevant to the reactor, and
could lead to dryout and pin failure with further gas release. This is
a possible fault propagation sequence which has not yet received attention.

4. Discussion: requirements of a local boiling model

It is clearly necessary for a local boiling model to be able to take into
account the hydrodynamic interaction between a growing vapour bubble and the
flow. Since boiling takes place in a region where there are steep velocity
and pressure gradients, there will be translational motion of the bubble centre.
In addition, at low super-heats (below about 6°C), distortions of the bubble
shape occur, which require consideration in the model.

The redistribution of hot fluid displaced from the wake as the bubble grows
is important in determining a) the mean temperature of the clad in steady
boiling, b) the temperature of the bubble interface and hence the evaporation/condensation near rebound when regrowth is possible. This is relevant both
to consideration of possible dryout and to acoustic detection of boiling.
It is evident that the complex coolant flow field may invalidate models which
make oversimplified assumptions about the displaced liquid.

The presence of non-condensible gas in a local boiling situation has been
shown to be capable of having a significant effect on boiling behaviour.
A model should treat this critical problem with some realism, since it appears
to be the most probable situation in which dryout, and therefore escalating pin
failure, could occur.
5. Implications for fast reactor safety

The protective instrumentation conceptually provided to protect the fast reactor core against a SPERT incident, is usually based on the premise that there will be significant signals at the sub-assembly outlet as a precursor to the incident. However, it is shown below that it is possible in certain circumstances for a local event to occur which is too small to produce detectable outlet mean flow or temperature signals, but which could involve sufficient quantities of molten fuel in a voided region to lead to a fuel-coolant interaction with an energy yield sufficient to endanger the whole core. Our understanding of boiling, and associated fuel behaviour, is not at present sufficient to confidently demonstrate that such events are more improbable than sub-assembly voiding.

To estimate the maximum geometrical extent of a local fault which would not affect overall flow and temperature significantly, two possible cases are considered:

(i) a thin, flat-plate blockage with a short voided wake,

(ii) a smaller fault, of greater axial extent e.g. a) a flat plate blockage with a long void downstream, or b) a blockage extended axially due to clad ballooning or pin bowing.

The outlet temperature and bulk flow perturbations for these cases are given in figures 7(a) and (b) for a typical sub-assembly, and indicate that for a detection threshold corresponding to a change in outlet flow or temperature of 10%, the amount of fuel which could be involved is $\approx 7\%$ of that in the sub-assembly for case (a) and $\approx 17\%$ for case (b). The maximum energy yields from a Hicks-Menzies thermodynamic calculation\(^1\) for $\text{U}_2\text{O}_3$ at 3450°K will therefore be in the range 1.3 - 3.1 MJ (using a somewhat conservative Hicks-Menzies yield of 250 J g\(^{-1}\)); and the upper part of this range is sufficient to severely damage the sub-assembly wrapper\(^2\).

It would be a desirable objective of any safety instrumentation programme to show that local events of such a size as discussed above could readily be detected during their development. It would appear to be necessary, if a flow/temperature trip is to be used, to set the trip thresholds at not greater than $\approx 5\%$ above normal, and this may require automatic adjustment of trip levels etc.

6. Conclusions

6.1 Temperature distributions in the wake of a local blockage in water have been measured and compared with a simple theory. The results indicate that temperatures in sodium can be derived straightforwardly from temperatures measured in water. However, insufficient data exists to check scaling predictions to reactor size, and a larger scale apparatus is being constructed for this purpose. It is observed that the effect on wake temperatures of blockage permeability depends strongly on whether the leakage flow is uniform over the blockage or in the form of local jets.

6.2 A study of boiling in the wake has given useful information about the hydrodynamic interaction of bubble and flow, and the behaviour of the displaced hot liquid. Injection of gas into the wake has been shown to produce a dramatic increase in bubble frequency which was associated with a stabilised gas void. In a reactor environment such behaviour could result in a significant probability of dryout and propagating pin failure.
6.3 It has been shown that the maximum size of a local fault which could remain undetected by normal outlet flow or temperature monitoring is sufficient for a resulting local fuel/coolant interaction to lead to damage propagating outside the sub-assembly.

Acknowledgement

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References


Appendix I

A characteristic temperature rise in the pin bundle is that rise which would take place over a distance of 1 cm in the normal, unblocked bundle; ie $Q/pcv$, where $Q \text{ cm}^{-3}$ is the volume power density, $v \text{ cm sec}^{-1}$ the mean coolant velocity and $pc \text{ j cm}^{-3} \text{OC}^{-1}$ the coolant specific thermal capacity. In the figures the normalised temperature shown, $\theta$, is obtained from the measured temperature rise, $T-T_o$, by

$$\theta = \frac{(T-T_o) pcv}{Q} \text{ cm.}$$

where $T_o$ is the temperature at the beginning of the heated section. It should be noted that the blockage is situated 12.5 mm up the heated section so that the effect of the upstream flow disturbance on wake temperature is accounted for.
Heated section of pins

8mm pitch

6.35 mm diam.

Heated Pins

Blockage

FIG. 1. DETAILS OF TEST SECTION.

Traversing thermocouples.

FIG. 2. NORMALISED TEMPERATURE DISTRIBUTION, Θ(r,z), WITH IMPERMEABLE BLOCKAGE.
FIG. 3. VELOCITY DISTRIBUTION AND RESULTING TEMPERATURE DISTRIBUTION IN THE SIMPLE MODEL OF A WAKE.

FIG. 4(a) DEPENDENCE OF WAKE TEMPERATURE ON BLOCKAGE MATERIAL.

FIG. 4(b) DEPENDENCE OF WAKE TEMPERATURE ON COOLANT DIFFUSIVITY.
FIG. 5. NORMALISED TEMPERATURE DISTRIBUTION FOR PERFORATED BLOCKAGE (approx. 13% residual flow)

FIG. 6. NORMALISED TEMPERATURE DISTRIBUTION WITH UNIFORMLY PERMEABLE BLOCKAGE (10% residual flow)
Fig. 7(a). Perturbation of subassembly temperature rise for short voided wake $L=2D$.

Fig. 7(b). Perturbation of subassembly temperature rise for a long void.
MEASUREMENT OF THE LIQUID METAL RESIDUAL FILM LEFT BEHIND COOLANT EXPULSION IN NARROW CHANNELS

H. M. Kottowski, M. J. Mol, A. Morandi, E. Pancaldi, A. Zurli
JRC EURATOM, Ispra (Varese), Italy
(*) University of Bologna, Italy

Introduction

As it is well known, liquid metals once heated up to boiling conditions tend to superheat. The consequences are: explosive evaporation and violent expulsion of the coolant. After this the cooling of the heating channels will be maintained by the evaporation of the liquid left behind on the wall during the expulsion. The temperature history of the vapour and of the heating surface which defines the duration of the cooling, depends strongly on the quantity of the liquid left in the film, on the onset of turbulence, and on the onset of droplet flow caused by the destruction of the film by the streaming vapour.

In order to obtain quantitative information on the hydrodynamics of the liquid metal film, which has been left behind a coolant ejection in a channel, the following experimental and theoretical program has been accomplished:

a) Measurement of the local and average film thickness as a function of:
   - overpressure $\Delta p$ between the (vapour) bubble and the blanket (i.e. velocity and acceleration of the coolant during the ejection),
   - diameter of the channel,

b) Measurement of the onset of turbulence

c) Development of theoretical equations for the calculation of the film thickness.

Description of the Experiments

Test Apparatus

The experiments were executed in test tubes of 3.6, 6.0, 8.0 and 10 mm diameter and pressure differences between the inlet and outlet of the test sections in the range from 0.26 bars to 2.7 bars. The liquid metal used was eutectic NaK.

Fig. 1 shows the test apparatus. The total length of the test section between the valve and the upper vessel was 2.10 m. For measuring the film thickness, 1.7 m of the test tube were instrumented by welding 9 pairs of voltage taps in distances of 200 mm. The spacing between the taps of one pair was 2 mm.
The instrumented part of the test section was connected to a Pb-battery of 24 V in series with a large resistance in order to guarantee a constant current through the test tube during the experiments.

The method of measuring the local film thickness is based on the measurement of the local change of the electrical resistance of the NaK-tube system. The accuracy and sensitivity of the film thickness measurement depend strongly on the wall thickness of the tube. The wall thickness we have chosen for the test sections was 0.1 mm. It can be demonstrated that for a film thickness of the size of the wall thickness the relative error of the measurement is in the range of 2%.

Before starting the experiments, the test sections were heated up to 350°C in order to assure complete wetting. The experiments were carried out at ambient temperature.

Instrumentation

The pressure difference between the lower vessel P1 and the vessel P2 was measured by a Mercury-U-tube. The change in local electrical resistance, which is a function of the film thickness, was measured at 9 positions by recording the voltage change between the two taps of a voltage tap pair. The ejection velocity was monitored by an E-M-flow meter (FLM) and the temperature distribution along the test section was measured by Ni-NiCr-thermocouples. All measuring points were recorded simultaneously with an UV-Trace-Recorder.

Execution of the Experiments

All experiments were executed at constant pressure difference between the pressure vessel P1 and the vessel P2. Both vessels were large enough not to affect their internal pressures during the experiments. The test section was separated from the vessel P1 by a fast acting valve. The valve was opened by a remote control system and closed automatically after a preset time. The
remote control system also started and stopped automatically the UV-Trace-Recorder before the opening and after the closing of the valve. For the repetition of the experiments the test section was filled from below via the NaK reservoir.

Experimental Results

Experiments were carried out for each test tube at pressure differences of 0.266 bar, 0.399 bar, 0.666 bar, 1.333 bar, 1.999 bar and 2.666 bar between the lower and upper vessel. Each experiment was repeated 10 times.

Table 1 (Annex 1) shows some characteristic experimental data obtained in the 6, 8 and 10 mm tube at various pressure differences \( \Delta p \) between the pressure vessel and the blanket vessel. \( \bar{v}, \bar{v}, \bar{b} \) are the statistical mean values of 10 repeated measurements and \( \sigma_\bar{v}, \sigma_\bar{v}, \sigma_\bar{b} \) are the corresponding standard deviations.

Fig. 2 and 3 show characteristic records of two experiments in a test tube of 6 mm: at \( \Delta p \) of 0.399 bar and 2.666 bar respectively. The influence of \( \Delta p \), which corresponds with the influence of the ejection velocity and acceleration, is clearly shown in these graphs. Fig. 2 shows the characteristic features of the hydrodynamics of the film at low velocity and low and nearly constant acceleration (see also Fig. 4). They can be analyzed in a way that the film left behind the moving liquid slug remains undisturbed for a (relatively) long time before undulation provoked by the flowing gas, starts. No liquid is removed from the film during the expulsion process. The hydrodynamic behaviour for a \( \Delta p \) of 2.666 bar is quite different. The onset of turbulence is shifting versus the break-off point of liquid slug-film and the removal of liquid from the film starts immediately after the film has been formed (Fig. 3). This hydrodynamic feature has been observed independently of the tube size. It has to be reminded that the mechanism of the film formation in tubes larger than 8 mm changes, which will be discussed later(2).

\* odd pressure values are due to conversion from mm Hg to bar.
Characteristic patterns of the measured velocity, acceleration and film thickness are shown in Fig. 4, 5 and 6. Fig. 4 and Fig. 5 are the typical picture of the monitored liquid slug velocity and acceleration as a function of both the ejection path, and ejection time for various $\Delta p$. At small $\Delta p$, for example, the alteration of the acceleration is small over a long ejection path, whereas at higher $\Delta p$ the voiding process starts with a high initial acceleration, decreases rapidly and increases again to a high value (Fig. 4). Fig. 6 shows the film thickness plotted versus the ejection path. The points on this diagram are average values obtained from 10 experiments. The dotted points indicate the minimum film thickness at the start of undulation as defined in Fig. 7. It could be observed only in experiments at $\Delta p$ of less than 0.7 bar.

A comparison between Figs. 4, 5 and 6 makes evident that the film thickness increases with decreasing acceleration and vice versa ($db/dt \leq 0$; $db/dt > 0$). This interdependence between the film thickness and the instantaneous expulsion velocity and acceleration is also shown in Fig. 8. These experimental
results demonstrate qualitatively that at "constant acceleration" (in the test case about 20 to 40 m/sec²) the film thickness is increasing with increasing velocity (up to 10 m/sec).

An alteration in the mechanism of the film formation in tubular geometry of \( d \geq 8 \) mm is indicated by the measured data obtained from the experiments in the 8 mm and 10 mm test tubes. Fig. 9 shows the film thickness versus the ejection path for an 8 mm test tube, and Fig. 10 those for a 10 mm test tube at various \( \Delta p \). There is no definite interdependence of the film thickness as was found for a 6 mm test tube.
Theoretical Approach

A mathematical analysis for determining the film thickness is extremely difficult and only possible with restrictions and considerable simplifications. When starting with the drawing up of a model of film formation behind ejected slugs, we have to distinguish between capillary and non-capillary flow patterns. Several authors (1,2,3,4,5,6,7) studied the hydrodynamics of large gas slugs in narrow channels and evolved criteria in terms of the Eötvos number. If the Eötvos number is below the critical one, we have a case of capillary flow.

\[ E_o^* = \frac{\Delta p \cdot g \cdot D_t^2}{\sigma} \]

The tube diameter, at which the transition from capillary to non-capillary flow occurs, is:

\[ D_t = \frac{\sigma}{\sqrt{\Delta p \cdot g}} \quad ; \quad a = \sqrt{E_o^*} \]

(\(\sigma = \) surface tension; \(\Delta p = \rho_l - \rho_g\); \(g = 9.81\); \(\rho_l = \) liquid density; \(\rho_g = \) gas density).

The critical Eötvos number published by various authors is within the range of:

\[ 2.88 < E_o^* < 4.2 \]

from which we can conclude that in case of our tests the test tube of 8 mm exceeded the upper bound of the critical Eötvos number.

The theoretical approach discussed in this paper is limited to capillary flow. The relationship between the film thickness \(\delta\) and the channel diameter \(D_t\), the velocity \(V\) and the acceleration \(a\) can be well represented by the following formula:
which has been used to plot the curves in the Figs. 11, 12, 13 and 14. The film thickness was calculated by means of the iteration method in time steps of $10^{-3}$ sec, and by applying Newton's law for the pressure loss to compute the acceleration $b$ and velocity $V$ as follows:

$$
2 P(t_n) - P_{oo} = \rho \ell (\ell - x)b + K_\ell \rho \ell \cdot V^2 + \rho \ell \cdot g \cdot (\ell - x) + \xi \cdot \rho \ell \frac{\ell - x}{2D_t} V^2
$$

which yields:

$$
b_{n+1} = \frac{P(t_n) - P_{oo} - K_\ell \rho \ell \cdot V_n^2}{\rho \ell \cdot (\ell - x)_n} - g - \frac{\xi_n}{2D_t} V_n^2 \quad ; (K = 1.5)
$$

$$
V_{n+1} = V_n + b_n \Delta t
$$

$$
x_{n+1} = x_n + \frac{V_{n+1} + V_n}{2} \Delta t
$$

$$
\delta_n = K^* \left( \frac{\mu \cdot V_n}{\rho \cdot b_n} \right)^{1/2} \quad \text{if } b \neq 0.
$$

The meaning of the used symbols is the following: $P(t_n)$ = vapour or gas pressure in the bubble as a function of time, $P_{oo}$ = vapour or gas pressure in the blanket above the liquid, $\mu$ = viscosity of the liquid, $\ell$ = length of the coolant channel, $x$ = ejection path, $\xi$ = friction factor of the liquid, $K$ = empirical factor.

For the computation the following simplifications were assumed to be valid:

a) $P(t_n)$, $P_{oo}$, $b$ and $V$ = constant during each time step;

b) $\xi$ = constant for the time steps employed and is determined by the empirical equation:

$$
\xi = \xi_0 + 8.6 \cdot 10^{-4} \left( \frac{\log \frac{Re}{31620} \cdot D_t}{0.28} \right) 1.75 \quad (8)
$$

with $\xi_0 = 0.3164$; $Re = Reynolds$ number;

c) the influence of the acceleration on the friction factor can be neglected;

d) the equation for the computation of the film thickness in case of $b = 0$ can
also be employed for laminar flow up to $\text{Re} = 3600$.

**Comparison of the Computed and Experimental Results**

The experiments were carried out at constant pressure differences between the lower vessel $P_1$ and the upper vessel $P_2$. The real gas pressure in the gas bubble behind the valve was unknown and the comparison of the experiments and calculations was made on the basis of the acceleration and velocity patterns.

In Fig. 11 the calculated curves of the film thickness, acceleration, velocity and ejection time as a function of the ejection path are compared with the measured data. The points are average values obtained from 10 experiments, as quoted before.

The vertical axis represents the following derived values and corresponding scales: acceleration $b$ (m/sec$^2$) = nominal scale; velocity $V$ (m/sec) = $10^{-1}$ nominal scale; ejection time $t$ (sec) = $10^{-4}$ nominal scale; film thickness $\delta$ (m) = $10^{-6}$ nominal scale. The horizontal axis represents the ejection path $x$ (m) = nominal scale.

The graphs showing the best agreement of the measured and calculated acceleration have been chosen for comparison. The measurements and calculations correspond remarkably well, especially as far as the pattern of the curves is concerned. The calculated film thicknesses, for example, are within 15% from the measured ones. The disagreement between the measured and calculated velocities could be provoked by the simplification in the computer code quoted under point c), and by the fact that $P(t)$ was unknown. Unfortunately the lowest voltage tap failed and the film thickness at the beginning of the ejection process could not be measured (the dotted circle represents only one measuring point).

A surprisingly good agreement between theory and experiment is presented in...
Fig. 12 for a test tube of 8 mm at low ejection velocity and acceleration (in the range where the computed and measured values coincide), although the test tube already exceeds the so-called "critical diameter" up to which capillary flow can still occur.

At the initial stage of the ejection process there is a great discrepancy between measured and computed values. This might be due to the vibration at the opening of the valve. On the other hand, there is experimental evidence, obtained from experiments with H₂O, glycerin and alcohol in capillary tubes that the film thickness starts with small values and increases with increasing velocity, which coincides with the computed curves. A quite different result was obtained at higher ejection velocity and ejection acceleration (Fig. 13).

At the beginning of the ejection we observe the same pattern of the film formation, but with increasing velocity and acceleration the variation of the velocity and acceleration has no detectable influence. The discrepancy between measurement and theory indicates the change in the hydrodynamics of the film formation, which confirms the theoretical prediction of the change from capillary
flow to non-capillary flow in tubes of more than 7 to 8 mm diameter. This interpretation is confirmed by the measurements in a test tube of 10 mm, which shows the same characteristics (Fig. 14).

![Fig.14-COMPARISON OF MEASUREMENT AND CALCULATION (TEST TUBE:10mm)](image)

**Conclusions**

- The experiments confirmed the theoretical prediction of the difference in film formation mechanisms between capillary and non-capillary flow. It could be proved that the change of the flow regime occurs in tubes of 7 to 8 mm diameter.

- In the capillary slug flow regime, the film left behind the ejected liquid slug increases at increasing ejection velocity and constant or decreasing acceleration of the slug, and decreases at increasing ejection acceleration. The influence of the velocity and acceleration on the film formation are counteracting.

- In spite of the simplifications made in the theoretical approach, the agreement between experiment and computation is good. The film thickness is well reproduced as a function of the ejection velocity and acceleration or, provided that the gas or vapour pressure in the bubble is known, as a function of the pressure difference between bubble and gas blanket.

- It could be shown experimentally that the onset of turbulence in the film and the transport of liquid into the gas flow start already at low velocities.

- The developed equations can be employed only for capillary flow regime. The question is, whether they can also be applied for a Na-cooled reactor geometry. This depends on whether the flow regime in the channels in case of an ejection accident is a capillary flow regime or not. Corresponding experiments are in preparation.
Nomenclature

$D_t$ = tube diameter (m) 
$g$ = 9.81 m/sec$^2$

$\ell$ = test section length (m) 
$\delta$ = film thickness (m) 

$x$ = ejection path (m) 
$\sigma^+$ = standard deviation 

$V$ = ejection velocity (m/sec) 
$\Delta t$ = time interval (sec) 

$b$ = ejection acceleration (m/sec$^2$) 
$P(t)$ = gas or vapour pressure as a function of time (N/m$^2$)

$\rho \ell$ = liquid density (kg/m$^3$) 
$\rho_{oo}$ = gas blanket pressure (N/m$^2$)

$\sigma$ = surface tension (N/m) 
$\zeta$ = friction factor 

$\mu$ = viscosity (kg/m, sec)

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LONG RANGE TARGETS CONCERNING THE Na-BOILING PROGRAM IN THE JRC - ISPRA

The Na-boiling program is directed toward the investigation of three forms of phenomena which dominate the picture during the boiling process. The following priorities have been observed:

a) Investigation of thermo-dynamic processes when the liquid reaches the boiling point: superheating, flow patterns, at the moment the boiling point is reached in superheating liquids, as well as parameters, which govern the superheating.

b) Investigation of stationary boiling: basically the following parameters are investigated: - flow patterns in the two-phase flow, heat transfer, superheating of walls, and the hydro-dynamic stability of the flow.

c) Critical heat flux during boiling: the investigations of this phenomenon are supposed to give information about the limits of thermal loads in systems cooled by boiling liquid metals.

As the influence of geometry and of the hydro-dynamic conditions on the phenomena of boiling are not sufficiently known, experimental investigations are to be conducted in both, a single channel, as well as in a heated bundle. By virtue of its simple geometry and its favourable possibilities of instrumentation, the single channel is best suited for the investigation of thermo-dynamic phenomena, whose understanding is essential for the evaluation of the boiling processes in the bundle. The investigations of both geometries are indispensable for the testing of correlations which describe heat transfer, critical heat flux, and pressure loss.

Current Research Program of the JRC Ispra

Theoretical investigations:

a) Mechanism of nuclei formation at the inception of boiling.

b) Estimate of the superheating, considering the parameters: contamination of the liquid metals, heating surface materials, roughness of surface.

c) Investigation of the influence of velocity on superheating.

d) Flow patterns and heat transfer during stationary boiling (started).

e) Critical heat flux during stationary boiling (started).

Experimental investigations:

a) Superheating at the beginning of boiling.

The investigation of superheating is being conducted with regard to two problems: 1) influence of chemical and mechanical surface factors on superheating, and 2) the influence of hydro-dynamic factors on superheating.

- In the laboratory tests the influence of various materials are being investigated. So are the machine state of the surface, and its chemical treatment. A detailed study is made, and long-range tests are being carried
- Materials used are for instance stainless steel, and nickel. Treatment of surface: roughness, coating with particles (spheres), meshes, and flame spray.

Chemical treatment: phosphatizing, nitriding.

These tests have been going on for years. For some operating conditions quantitative results are available.

- Objective of the experiments with forced convection is the measurement of influences of hydro-dynamic conditions on the superheating.

The measurements of the hydro-dynamic influence on the superheating of sodium are being conducted up to an Re-value of 180 000. Then they are being evaluated with regard to the superheating of the walls in the place of the inception of boiling, and to the medium superheating of the cooling agent following its emergence from the heated zone. Two testing methods are being applied in order to eliminate possible influences from the methods as such:

1) Constant hydro-dynamic conditions and a gradually increasing heat flux.
2) Constant heat flux and constant hydro-dynamic conditions: alteration of the thermo-dynamic balance until boiling point is reached via alteration of system pressure.

b) Investigation of heat transfer and the critical heat flux.

The objective of this investigation is the experimental determination of the heat transfer during stationary boiling of liquid metals with forced convection. The variables are velocity and system pressure (0.5 - 2 bar). Besides this investigation of the critical heat flux, i.e. of the heat flux and velocity correlation, where the liquid cooling of the heating wall is eliminated, the flow patterns and the heat transfer are investigated up to the critical heat load. So far, experiments have been conducted up to a flow velocity of 2 m/sec in a channel of 9 mm diameter, varying the heat flux up to the dry-out point.

Additional Studies on the Parameters

In order to improve the interpretation of the mechanism of heat transfer, and the hydro-dynamics of the two-phase flow of boiling liquid metals, a number of the more dominating parameters are investigated in simulation tests.

a) Measurements of film thickness:

Tests have shown that the multi-slug form of flow is dominating in boiling liquid metals. This results in heat exchange between the wall and the two-phase flow largely by evaporation of the film.

With this the hydro-dynamics of film formation, and the film flow to the two-phase flow are becoming decisive factors. As to the film formation, we have to discern (as a matter of principle) between the film formation behind moving liquid cylinders in capillary and non-capillary channels. Additional geometry parameters are for instance the spacers or diaphragms in tube channels, and the geometric cross section form, as for instance in
bundle geometries.

The experimental program of film investigation is aiming at the investigation of the following parameters:

1) Influence of the channel diameter (ideal tube geometry) on film formation,
2) Influence of the velocity and of the acceleration on film formation, and the hydro-dynamic processes in the film (investigations of the above two problems have been concluded in tubular geometry),
3) Formation of film before and behind spacers and diaphragms (studies being conducted at present),
4) Influence of cross section geometry.

The aim of these investigations is an answer to the question whether in a tightly packed bundle of elements of about 6 mm in diameter, capillary structure is to be presumed for the film formation or not.

b) Hydro-dynamic behaviour of solved gases in liquid metal circulation systems: (in collaboration with UKAEA Risley and Belgonucléaire)

The experimental investigation of the hydro-dynamic behaviour of gas bubbles in Na-cooling systems is limited to:

1) the behaviour of large gas bubbles in narrow channels and
2) the behaviour of micro bubbles in liquid metal circulation systems (gas contents about 10 ppm) and the possibilities of their detection.

The investigation of the behaviour of large gas bubbles under isothermic flow conditions has been concluded, while the investigation of possibilities of detection, and the behaviour of micro bubbles under isothermic and non-isothermic conditions is presently being prepared.

c) Investigation of two-phase flow of boiling Na in multi-rod geometries:

Experimental investigations on small multi-rod geometries are planned for the support and reinforcement of theoretical studies on heat transfer, and the hydro-dynamics of the two-phase Na-vapour liquid flow. These experiments are a necessary adjunct to the one-channel tests. The same phenomena are supposed to be measured here as in the one-channel system.

These are then supposed to be compared with the developed correlations.
SODIUM EXPULSION TESTS FOR THE SEVEN-PIN GEOMETRY

by

R. E. Henry, R. M. Singer, G. A. Lambert, L. M. McUmber,
D. J. Quinn, E. A. Spleha, E. G. Erickson,
W. C. Jeans, and N. E. Parker
Argonne National Laboratory
Argonne, Illinois 60439

ABSTRACT

Sodium expulsion data are reported for single- and seven-pin geometries with LMFBR heat flux conditions following pump coastdown flow transients. Analysis of the data shows that in both cases boiling inception occurred at the end of the heated zone with a small superheat. Net positive flow was observed during the early voiding stage and the general flow pattern appears to be annular.

I. INTRODUCTION

In order to analyze the liquid-metal coolant behavior in a liquid-metal fast breeder reactor following a loss-of-flow transient, it is necessary to understand several basic aspects of boiling liquid metal systems and their relationship to LMFBR conditions. These phenomena include incipient boiling superheat, boiling flow regimes under high heat flux conditions, local pressurizations due to voiding, and condensation on cold regions outside of the heated zone. To provide basic data on the behavior and relative importance of such phenomena under LMFBR conditions as well as to provide basic guidance regarding what physical assumptions can be made in analyzing such processes, voiding (expulsion) data have been obtained in the OPERA facility for pump coastdown flow transients under full power conditions. These results have been obtained for a single pin annular flow geometry and for a seven-pin configuration which was a full length prototypic simulation of FFTF fuel pins. These results have produced a detailed insight into the early voiding stages under LMFBR conditions and as such provide not only basic information on the flow boiling processes involved but also provide much needed resolution on the early voiding stages of several integrated in-pile experiments.

II. EXPERIMENTAL APPARATUS

The OPERA facility is a transient flow apparatus which provides an excellent simulation of the hydraulic conditions (inlet and outlet pressure) necessary to conduct prototypic LMFBR loss-of-flow experiments. Figure 1 is an isometric view of the OPERA facility which is a blowdown system involving a high pressure vessel with a 12 cu ft volume, capable of sustaining ~200 psig at 1200°F, and a low pressure receiver vessel with a 15 cu ft volume that can sustain 20 psig at 1600°F. As shown in the illustration, the test sections run vertically upward from the bottom of the blowdown vessel into a dogleg in the receiver vessel. Within the receiver vessel there is a 2 ft standpipe directly above the exhaust port to the test section to insure a fixed sodium height at all

*Work performed under the auspices of the U. S. Atomic Energy Commission.
times. An experimental run is conducted by pressurizing the blowdown vessel to a prescribed level, setting the pressure in the receiver vessel to the desired value, and then opening the control valve at the bottom of the test section allowing the sodium to flow vertically upward through the test bundle. Once full flow is established, the high flux electrical heaters used to simulate the nuclear fuel pins are energized and brought up to full power. When full flow and full power are obtained, the conditions are sustained for a short period of time to insure that all thermal profiles are fully developed, and then the flow transient is initiated.

For the pump coastdown type of flow transients which have been studied to date, the flow decay is generated by venting the gas pressure off of the blowdown vessel through a series combination of a control valve and a converging-diverging nozzle. This particular system allows for a broad range of coastdown rates and the coastdown rate for a given test can be reliably preset by extensive adiabatic testing prior to the actual experimental run itself.

Basic system instrumentation includes internal thermocouples in both vessels at various axial locations, two differential pressure transducers at the bottom of each vessel, absolute pressure transducers and gauges on each pressure vessel, and assorted monitoring thermocouples throughout the system. The advantage of this instrument configuration is that it is essentially self-contained, i.e., the internal thermocouples within the vessel can be used to monitor the sodium height within the vessel and thus provide a calibration for the differential pressure transducers for each vessel and these calibrations can then be used to calibrate the inlet and outlet flowmeters used on the individual test sections. This means that all the essential instrumentation can be calibrated into place and at temperature.

III. SINGLE PIN TEST

The single-pin test section and some of its basic instrumentation is shown schematically in Fig. 2. Sodium from the blowdown vessel passed through the inlet flowmeter and into the lower plenum of the test section where the local pressure and temperature are recorded. It then flowed vertically upward through the annular space between the wire-wrapped single pin and the outer wall of the test section. Basic parameters of the test section and the specific tests are listed in Table 1. The instrumentation for this test included inlet and outlet flowmeters, temperatures, axial temperature measurements along the outer wall of the test section, and voltage taps along the outer wall in the region of the downstream end of the heated zone and the gas plenum region immediately downstream. This instrumentation allows excellent experimental resolution of the transient single-phase behavior during the flow coastdown, the axial and radial subcoolings just prior to incipient boiling, the location of boiling inception, liquid-vapor boundaries following boiling inception, local void fractions during the voiding process, and the relationship between test section flow and these individual local void fractions. Such resolution is necessary in order to comprehend the basic two-phase characteristics of the voiding process.
Calibration
Following the calibrations of the electromagnetic flowmeters, pressure transducers, and thermocouples, adiabatic blowdown tests were conducted to establish the flow decay curve. In order to preserve the high flux heater, energy balances on the test section were the final checks in the calibration procedure. At bulk temperatures of 480°C, a 97% energy balance was achieved. For higher temperatures, the energy balance was slightly worse. Calibration runs were also taken to determine the frictional pressure drop characteristics of the test section. The total pressure drop (ΔP_t) experienced between the inlet (P_i) and outlet (P_o) pressure transducers is a combination of losses due to acceleration, wall shear, expansion, and head drop. By using one-dimensional approximations and negligible pressure recovery at all area expansions, an estimate of the Moody friction factor can be obtained for the wire wrapped region of the test section. This can be described by

\[ f = 0.175 \frac{N \cdot Re}{D} \]  

To provide assurance that the voltage taps were properly sequenced and operating as expected, a gas injection line was installed at the test section inlet. By injecting gas into the test section when it is filled with liquid and monitoring the voltage tap output, any anomalous behavior is readily apparent.

### Table I. Test Section Dimensions

<p>| | |</p>
<table>
<thead>
<tr>
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<tbody>
<tr>
<td>Heater OD</td>
<td>0.230 in.</td>
</tr>
<tr>
<td>Heated Length</td>
<td>22.5 in.</td>
</tr>
<tr>
<td>Unheated Gas Plenum Region</td>
<td>7.5 in.</td>
</tr>
<tr>
<td>Wire Wrap Diameter</td>
<td>0.070 in.</td>
</tr>
<tr>
<td>Wire Start Diameter</td>
<td>0.070 in.</td>
</tr>
<tr>
<td>Outer Wall ID</td>
<td>0.375 in.</td>
</tr>
<tr>
<td>Outer Wall OD</td>
<td>0.500 in.</td>
</tr>
<tr>
<td>Flow Area (wire wrapped region) (neglecting wire starts)</td>
<td>0.065 in.²</td>
</tr>
<tr>
<td>Wetted Perimeter (wire wrapped region) (neglecting wire starts)</td>
<td>0.000451 ft²</td>
</tr>
<tr>
<td>Hydraulic Diameter (neglecting wire starts)</td>
<td>2.12 in.</td>
</tr>
<tr>
<td></td>
<td>0.176 ft</td>
</tr>
<tr>
<td></td>
<td>0.000451 ft²</td>
</tr>
<tr>
<td></td>
<td>0.123 in.</td>
</tr>
<tr>
<td></td>
<td>0.010 ft</td>
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</tbody>
</table>

Experimental Conditions
Since the test section was a short single-pin geometry the experiment differs considerably from a reactor subassembly configuration. The basic differences are the amount of coolant per pin and the amount of unheated structural material in relationship to the heated length. To minimize the effect of these differences, an elevated inlet temperature and reduced steady-state velocity were used. Both of these conditions raised the steady-state exit temperature level and thus decreased condensing capacity of the unheated structure during the voiding transient. This reduced steady-state velocity and its relationship
to an FFTF coastdown is shown in Fig. 3 along with the resulting coastdown used in the OPERA single pin test. Effectively, the FFTF coastdown is picked at 10 sec into the transient as far as a single pin test is concerned. Therefore, all the data which are presented in this paper are related in time to the FFTF coastdown as shown in Fig. 3.

The inlet temperature for this test was 571°C and the heater power was held constant at 20.1 kw throughout the test. These conditions along with the initial test section velocity correspond to a steady-state temperature rise of 167.8°C which gives an energy balance check within 93%. For the initial velocity shown in Fig. 3, the test section inlet pressure was 1.20 atm and the receiver vessel pressure was held constant at 0.68 atm during the coastdown. These conditions result in a pressure at the downstream end of the heated zone of 0.89 atm and this value is essentially constant throughout the flow transient.

In order to estimate the incipient wall superheat at the onset of voiding, as described in Ref. 1, it is necessary to know the pressure-temperature history of cavities on the heated wall as well as the pressure-temperature history at the sodium-argon interface.

**Gas Interface**

High purity argon was the only gas used in the entire system, and for all periods other than the short times (10 min) required for calibration runs, the system was maintained under an argon blanket at approximately 1.3 atm. Over a period of weeks, the bulk temperature was gradually increased up to 482°C, and this temperature was maintained for two weeks prior to the test. Therefore, it is reasonable to assume the sodium is saturated with argon gas at these conditions. One day before the test the temperature was increased to 571°C. In this short time, it is unreasonable to assume that the argon could diffuse through and saturate the 3.3 m column of sodium in the blowdown vessel. However, bounding calculations can be run with these numbers.

**Wall Cavities**

Care was taken during the calibration procedures to avoid any deactivating condition that would be more severe than those experienced during the conduction of the test itself. The following sequence is believed to produce the most severe deactivation for this test.

1) Steady-state, isothermal (571°C) flow was initiated with sodium that was saturated with argon gas at 482°C and 1.3 atm.

2) The power was slowly increased over a period of minutes until the full power condition was achieved.

For the above conditions, the partial pressure of argon gas at 571°C is 0.232 atm. Using Bankoff's reentrant cavity model a maximum active radius of 0.00046 cm is calculated. Assuming the noncondensable gas in the cavity behaves in a constant volume manner as the test section temperatures increase during the transient, the gas partial pressure at activation is 0.31 atm. For a pressure of 0.89 atm at the end of the heated zone, a temperature can be calculated from the reentrant cavity model such that the cavity attains the limit of stability.
This temperature is 891°C compared to local saturation value of 868°C. This estimated value of 23°C incipient wall superheat is consistent with benign expulsion witnessed at inception, and for the conditions of this test and a Nusselt Number of 6, it corresponds to zero bulk superheat.

**Discussion of Results**

The most definitive instruments for detection of boiling and for mapping the voiding picture are the voltage taps. These voltage taps, which are driven by a small (10 ampere) dc current which is passed through the sodium coolant and the outer wall, enable one to detect the location of liquid-two-phase interfaces. It is important that the distinction be made between liquid-vapor and liquid two-phase because when voiding is initiated, the voltage taps primarily indicate a departure from the all-liquid condition. Therefore, temporal signals from the voltage taps can be utilized to describe axial regions of the coolant channel that are in an all-liquid condition and which ones are not. Such a voiding picture for this test is illustrated in Fig. 4. To help orient the reader, a schematic of the test section is shown on the left in the figure.

![Fig. 4. Single-pin voiding profile.](image1)

![Fig. 5. Single-pin inlet flowmeter data.](image2)

Inception occurred at 17.40 sec at the end of the heated zone. This small void, which is barely distinguishable from the all-liquid case, then collapses and a second inception is experienced at the end of the heated length. This nucleation produces vapor which progresses about 8 cm downstream before it collapses. At 17.66 sec, boiling inception occurs approximately 4.4 cm upstream from the end of the heated length. For the next second, several more nucleation sites successively become active and drive liquid slugs up the channel. Meanwhile, vapor bubbles are progressing further and further down before condensing and thus warming up the outer wall and the unheated portion of the heater.

A comparison with the inlet flowmeter data for this test which is given in Fig. 5 shows that there is net positive flow until about 19.30 sec into the coastdown. In addition, it shows that the perturbations experienced by the inlet flow are of little consequence until 18.8 sec at which time the flow decreases rapidly. From the void profile shown in Fig. 4, it is seen that, at 18.8 sec into the coastdown, the unheated downstream section has achieved saturation as have regions 8 cm upstream from the end of the heated length. This large voided (two-phase) region causes the frictional pressure drop to increase dramatically,
which results in a sharp decrease in the inlet flow, and thus, sets the stage for inlet flow reversal. This demonstrates that there is an excellent one-for-one correlation between the flowmeter and voltage tap results.

It should be reiterated that the results shown in Fig. 4 simply describe the boundaries between what is all liquid and what is not all liquid. To illustrate this, the sketch on the right shows an instantaneous flow distribution, as indicated by the voltage taps at 18.30 sec, as it might appear in a circular channel. The most important aspects of this illustration are the multibubble character of the flow and the small local void fractions.

Figure 6 displays the outlet flowmeter results during the voiding transient. In contrast to the inlet flowmeter, a small but distinct reaction to inception is apparent as is the small decrease in flow as the small void is collapsed. At approximately 17.6 sec into the coastdown, another expulsion is recorded which is in agreement with the voltage tap results. Here again the excellent correlation between the flowmeter results and the voltage tap data is evidenced.

Fig. 6. Single-pin exit flowmeter data. Fig. 7. Single-pin axial temperature profiles.

The temperature measurements shown in Fig. 7 do not reflect any rapid thermal transients in the coolant channel because of the sizeable thermal time constant of the outer wall. However, such data are representative of the average behavior between the flow transient and present a picture of the voiding process that is consistent with the voltage tap results given in Fig. 4. The agreement between the outer wall temperature rise at t = 0.0 sec and the steady-state energy balance demonstrates the level of confidence that may be placed in these measurements. (The rise in temperature after the end of the heater zone is due to the measurement being an outer wall type instead of a true bulk measurement.) During the flow decay, the outer wall temperature rose rapidly with the end of the heater zone having the maximum temperature. At inception, coolant and wall subcooling increases in both directions from the end of the heater zone with the rate of increase being far greater in upstream direction. Therefore, as voiding begins, the void progresses preferentially downstream because net positive flow is maintained and because the subcooling is much less in that direction. As illustrated in Fig. 7, this movement of the vapor quickly reduced the subcooling of the unheated downstream portion (t = 18.6 sec) so that this entire region is then voided as shown in Fig. 4. A comparison with the outlet thermocouple results
for the same times shows the extreme subcoolings downstream of the heater. However, this is of little consequence because the region with the small hydraulic diameter, namely, the downstream unheated portion of the annular coolant channel has been voided at $t = 18.6$ sec and the large two-phase pressure drop in this region means that it has become very difficult to transport the vapor generated in the heated region out to where the cold structure and coolant are located. The dramatic increase in the two-phase frictional pressure drop is known as flooding and the fact that such a phenomenon exists can be illustrated by a simple calculation. As shown in Ref. 5, the onset of flooding for a given system is determined by the liquid and vapor velocities. For this demonstrative calculation, the liquid velocity can be neglected in which case the correlation for turbulent flow in both components is simply

$$0.6 = u_v \left[ \frac{\rho_v}{gD_h (\rho_L - \rho_v)} \right]^{1/2}$$

(2)

For the conditions of this experiment, flooding would begin at a vapor velocity of about 5.8 m/sec. If it is assumed that all the energy generated per unit length in the heater is used to generate vapor over the length of voided heater region, the vapor flow rate is given by

$$W_v = \frac{q_h}{h_{fg}}$$

(3)

and if it is assumed that all the vapor flows downstream, then from continuity

$$u_v = \frac{W_v}{\rho_v \alpha A}$$

(4)

From Fig. 4, the voided heated length at 18.6 sec is 3.3 cm, and if the local void fraction is assumed to be 1.0, the vapor velocity is 30 m/sec which means the downstream unheated region is experiencing the flooding phenomenon if one-fifth of the vapor generated travels downstream. Therefore, the saturation of the unheated downstream region, which has occurred at about 8.6 sec into the coastdown, is the paramount condition for establishing downward voiding and thus inlet flow reversal. In regard to Figs. 4 and 7, after downstream saturation and inlet flow reversal were achieved, multibubble flow patterns were no longer witnessed.

In the above discussion, it was assumed, for the sake of calculation, that the local void fraction was 1.0. Selected voltage tap signals were recorded on magnetic tape along with the necessary calibration data. If it is assumed that none of the liquid in a given interval exists as a discrete globule or droplet, then the voltage tap recordings for that interval can be reduced to average void fraction data for that locale. Since the dc current was constant with time,

$$\frac{\Delta V_{2\phi}}{\Delta V_E} = \left[ \frac{R_{NaF}}{R_{NaF} + (1 - \alpha)R_{St}} \right] = \left[ 1 + \frac{(1 - \alpha)R_{St}}{R_{NaF}} \right]^{-1}$$

(5)

The structural resistivity is calculated from the single-phase signal just prior to inception assuming the liquid sodium resistivity corresponds to the value at saturation.

If local void fractions are compiled at specific times, axial void fraction profiles can be generated. Such profiles are presented in Fig. 8, at four different times in the voiding transient. The behavior is in good agreement
with the outer wall thermocouple results shown in Fig. 7, i.e., the early voiding causes the downstream region to quickly saturate, as evidenced by the almost uniform void profile in that region, which results in a high frictional pressure drop path for the vapor. This is a necessary condition for producing inlet flow reversal. The dip in the void profile at the end of the heated length is due to a wire start that spans this interval. This wire start presents two more unheated walls to retain a sodium film, hence, the lower void fraction. That such is the case can be verified by calculating the liquid fraction, which is unity minus the void fraction, at any time and compare the value for the end of the heated length with any other interval. For times after flooding, the liquid fraction at the end of the heated length is twice that for the other intervals.

When the heater was extracted from the system, its appearance was exactly the same as that prior to the test. No discernable damage could be detected on the outside of the heater. Radiographs of portions near the end of the heated length show a simple melting of the nichrome-V element. However, it is also apparent that considerable melting occurred at other axial locations. From the locations of the melted regions and that of the wire wrap, it is reasonable to conclude that the melting was preferentially oriented away from the wire wrap. This is another strong indication that the wire wrap retains a sodium film which provides considerable cooling for that location well into the voiding transient.

IV. SEVEN-PIN TEST

The seven-pin test was designed to obtain axial voiding data and voiding rates characteristic of a 217-pin fast reactor subassembly following a pump coastdown loss-of-flow transient. Available seven-pin sodium data, such as that taken by Collingham, clearly indicated that for a seven-pin hexagonal geometry with full scale wire wraps on all seven pins, large radial temperature gradients could be anticipated. For large subassembly geometries, such gradients would only be anticipated on the very outside edge of the bundle, and the center would exhibit a flat radial temperature profile over perhaps six rows of pins. Therefore, in order to run a definitive test, it is essential that the boiling behavior (voiding) for the seven pin test section should be indicative of the large reactor subassembly geometry. Since the center of the subassembly comes to saturation almost uniformly for a pump coastdown flow transient, it is felt that this region will indeed govern the boiling stability and voiding behavior within a subassembly geometry. Hence, it was felt that the seven pin geometry should be configured to minimize radial temperature differences due to mismatches between power to flow ratios in the outer subchannels of the test section. To evaluate various test section designs, a very simplistic thermal-hydraulic inner channel mixing model was formulated. This model, entitled, SIMPLE, allowed a comparative evaluation of test section alterations such as reduced outer wire wraps and filler wires along the outer channels. The model itself checked out well with available data for seven, 19, 91, and 217 pins, and thus gave a high level of confidence that it could be used as the evaluation model for seven pin wire wrapped test section designs. Design calculations reveal that the geometry shown in Fig. 9 would yield approximately equal power-to-flow ratios for all subchannels involved. As an experimental check against these design calculations,
the filler strips used in the test section itself were chromel-alumel thermocouples that extended the entire length of the hexagonal geometry and whose junctions were located in close proximity to the downstream end of the heated zone. Significant deviations from the design calculations would then be indicated by these thermocouples since power-to-flow mismatches are most prevalent in these particular subchannels.

Figure 1 illustrates the seven-pin test section as it was mounted in the OPERA facility. It clearly shows inlet and outlet flowmeters as well as the inlet and outlet plenum configuration for this test. The test section instrumentation, in addition to the flowmeters, consisted of inlet plenum pressures, six internal thermocouples as shown in Fig. 9, 25 outer wall thermocouples along the axial length of the test section, 25 voltage taps along the outer wall of the test section to measure locations of liquid-vapor interfaces, and individual power inputs to all seven pins. The pertinent physical dimensions of the heater pins themselves, their attached gas plenum simulators, and their configuration within the seven-pin test section itself is illustrated in Fig. 10.

Since the high flux heaters were all grounded to the sodium coolant at the downstream end of the heated zone, the electrical power could be provided by two three-phase Y systems and one single-phase circuit. A given three-phase circuit supplied three adjacent pins in the outer row and the single-phase circuit supplied the center pin. During the actual transient, the current from the center pin was used to drive the voltage taps (void probes).

**Calibration**

The instrument calibrations for test section flowmeters, pressure transducers, and thermocouples were conducted in the same manner as for the single pin test. Since the test section was designed to give equal power-to-flow ratios...
in all subchannels, the coastdown rate itself could be set equal to that of FFTF instead of altering the actual coastdown as was done for the single pin experiment. The final calibration test in the sequence was an energy balance at full flow and full power with a 315°C test section inlet temperature. This energy balance gave a check within 98% of the total energy input to the test section and this was considered satisfactory.

In the course of the calibration tests, the frictional pressure drop through the wire wrapped seven-pin configuration was determined in the same manner as discussed in the single-pin test, and the same functional dependence between friction factor and Reynolds number as given in Eq. 1 was found for the seven-pin geometry.

Voltage tap sequencing and other anomalies were checked by gas injection into the test section and by filling and draining the section with a small dc current being applied to the seven-pin bundle. These calibration checks showed the voltage taps to be operating in a normal manner.

**Experimental Conditions**

Given the basic design of the seven-pin test bundle, the sodium coolant per pin was essentially that of a FFTF subassembly. Therefore, prototypic inlet temperatures and coastdown rates could be directly employed to this experiment. Figure 11 details the actual decay utilized in this study. The test section inlet temperature was 315°C and the heater power was held constant at 11 kw/ft for all seven pins and was uniform over the 3 ft axial length of the heated region. The initial test section inlet pressure was 8.6 atm and the receiver vessel pressure was held constant at 1 atm during the entire flow transient.

![Fig. 11. Seven-pin pump coastdown flow transient.](image)

Given this experimentally measured information, it is possible to formulate a prediction for the boiling inception wall superheat in this test.

**Gas Interface**

As with the single pin test the only gas applied to the system was high purity argon and, for all periods other than the short times required for calibration runs, an argon blanket was maintained at 1.4 atm. Over a period of weeks temperatures in both vessels were raised to approximately 315°C, and these values were maintained for 24 hrs a day except for the short time calibration runs. Therefore, a reasonable assessment of dissolved argon gas concentration in the liquid sodium, and thus in the surface cavities in the heated region, is approximately 1.4 atm at 315°C. For the short operating times involved in the calibration runs and in the test itself, it can be assumed that no argon gas diffused out of the cavities.

**Wall Cavities**

For the gas interface conditions given above, the most severe cavity deactivating conditions are produced when full flow is generated in the test bundle at a temperature of 315°C with a 1.4 atm noncondensible gas pressure in the wall surface cavities. Under full conditions, the pressure at the end of the heated zone is approximately 4.8 atm. Since the time at full power and the flow coastdown transient itself represent a total time of less than 90 sec, it is assumed that the gas within the surface cavities cannot diffuse any significant amount in this time. With the above deactivation conditions, Bankoff's reentry cavity model predicts the maximum active radius of 0.000089 cm. During the transient
it is assumed that the gas in the cavity behaves in a constant volume manner as
the test section temperatures rise, which yields a gas partial pressure activation
of 2.1 atm. The pressure at the end of the heated zone at boiling inception
was approximately 1.45 atm, and for such system pressure conditions the
reentry cavity model predicts the limit of stability is obtained at a wall tempera-
ture of 957°C. This corresponds to an incipient wall superheat of 33°C which
is in agreement with the initially benign voiding measured in the test, and it
also corresponds to approximately 0° bulk superheat.

Discussion of Results

Boiling inception, as indicated by flowmeter measurements and local voltage
tap measurements, occurred at 17.5 sec into the coastdown and at the downstream
end of the heated zone. Figure 11 shows the relationship between inlet and
outlet flowmeters following inception. In comparison to the single pin results
discussed above, the most striking feature of this voiding transient is the
relationship of the exit and outlet flowmeters. Whereas in the single pin tests,
voiding resulted in spikes in the exit flowmeter with effectively no reading on
the inlet flowmeter for 1 sec into boiling, voiding in the seven-pin geometry
is accomplished with both flowmeters exhibiting flow decays and the inlet flow-
meter decaying at a faster rate than the exit. Voiding in the seven-pin con-
figuration is remarkably smooth compared to the large flow oscillations demon-
strated in the single pin test. One major aspect which is the same for both
the single and seven-pin tests is that net positive flow is measured well into
the voiding transient and upstream voiding is still accomplished under this net
positive flow. The lack of oscillation on the exit and inlet flowmeter indicates
that radial heat sinks have indeed been minimized so that rapid growth and col-
lapses, due to voiding and rapid local condensation, have effectively been
eliminated. This particular aspect is further documented by the voiding profile
shown in Fig. 12. This particular profile can be contrasted with the single pin
data shown in Fig. 4 in which a multibubble type of flow pattern was observed
during the early portions of the voiding transient. As for the seven-pin test
the voltage taps reveal the voiding was extremely smooth and well behaved with
a downstream liquid vapor interface progressing far more rapidly than the up-
stream phase boundary. The primary reason for the smooth and rapid voiding can
be seen in Figs. 13 and 14 which present axial temperature profiles along the
outer wall early in the coastdown transient and at boiling inception respective-
ly. The triangles represent the internal thermocouples within the test section
itself and the remaining data represents temperature measurements on the outside
wall of the hexagonal can. The solid line shown in the figures represents a
simple one-dimensional steady-state
heat balance calculation illustrating that the measurements along the outer
walls during the pump coastdown tran-
sient, which is essentially a quasi-
steady process, are in good agreement
with such a prediction. If large radial
temperature gradients did exist, the
outside channels would run colder than
the center channels and consequently
a one-dimensional heat balance would
predict values higher than those mea-
sured on the outer wall. However, the
agreement between the one-dimensional
heat balance and the temperature mea-
surements on the outer wall indicate
that radial temperature gradients have
been greatly reduced and that the
process is essentially one-dimensional.

Fig. 12. Seven-pin pump coastdown
voiding profile.
As with the single-pin experiment, the voltage taps (void probes) can be used to obtain estimates of local void fractions as a function of time during the voiding process if it is assumed that the sodium exists in continuous axial filaments over the interval between two voltage taps. The taps themselves were concentrated near the end of the heated zone which was the hottest region within the test bundle and was thus the point at which inception was expected. The taps were then spaced up and downstream from this point at ever increasing increments over the whole length of the test section. Figure 15 illustrates the experimentally measured local void fractions versus time during the voiding transient. As shown in Fig. 11 inception occurred at 17.5 sec into the coastdown and at the end of the heated zone. The void fractions in this region immediately following inception were quite low and the flow through the test section was in the net positive direction. Voiding progresses downstream until it exits from the last voltage tap at approximately 18.5 sec. From this time on, the upper liquid vapor interface must be estimated from the integrated output of the inlet and outlet flowmeters, the location of the lower liquid vapor interface, and an estimate of the average void fraction within the voided region at any point in time. Inlet flow reversal occurs at 19.0 sec, and the void fraction measurements in a downstream gas plenum region essentially reach constant values. This essentially means that once inlet flow reversal has been accomplished there is no efficient means of transporting vapor downstream into the gas plenum region, and that the resulting sodium vapor generated within the core is expelling the liquid into the lower inlet plenum. After inlet flow reversal has occurred the lower void boundary can be estimated by assuming that the inlet flowmeter itself is recording the flow out of the core and by assuming an average void fraction, based on the local measurements, within this voided region. Such estimates show that
approximately 19.6 sec into the coastdown the entire core and fission gas plenum region is voided.

As shown in Fig. 11, the outlet flowmeter has a very smooth decay until approximately 19.5 sec at which time a very slow expulsion is observed. At this same instant, the voltage taps in the immediate region downstream from the end of the heated zone also demonstrate vigorous oscillatory behavior. Since the electrical heaters having nichrome-V element with a 316 stainless steel sheath, which both have essentially the same melting point, the element would achieve a molten state while the cladding was still in solid form. The heaters are fabricated in a 1 atm argon environment at standard conditions and then swaged to compact the boron-nitride between the element and heater sheath. Internal gas pressure after swaging is unknown, but if it is assumed to remain at 1 atm and if a constant volume process is assumed, the enclosed gas pressure within the heater would increase to approximately 4 atm at the sodium boiling temperatures measured in this study. Therefore, a rupture of the 316 heater sheath (cladding) at these elevated temperatures is a definite possibility. A post mortem examination of the test section revealed three heaters which appeared as if they had ruptured due to internal pressure. If such a rupture of the cladding did occur, gas would be released along with the boron-nitride and perhaps some molten nichrome-V from the heater element. The gas release should be seen simultaneously on the inlet and outlet flowmeters, and indeed it was as illustrated in Fig. 11. Frozen metallic material was found immediately downstream from the end of the heated zone in the 6 in. inconel plug region and also immediately upstream from the failure point. Although full power was still applied to all pins at this junction, total failure of the test bundle was imminent and it occurred at 20.5 sec into the coastdown. Therefore, in this seven-pin pump coastdown voiding transient, the first 2 sec of test section voiding are due to boiling, with the latter stages representing the combined effects of boiling and gas release.

Conclusions

From these two voiding tests, several conclusions can be made regarding the early phases of the coolant boiling process.

1. Incipient wall superheat is comparatively mild, and voiding can be assumed to begin when bulk saturation of the coolant is achieved.

2. When large radial and axial heat sinks are present, the early voiding pattern will be characterized by a multibubble type of flow.

3. For prototypic type radial and axial heat sink conditions, no multibubble type of flow patterns are observed and the voiding is a very smooth and well behaved phenomena.

4. During the early stages of the voiding transient, net positive inlet flow was measured in both the single and seven pin configurations.

5. The flow pattern observed during the early voiding transients can be categorized as a standard annular flow pattern as applied to pin bundle geometry.

6. After the downstream gas plenum region has achieved saturation, as demonstrated by essentially uniform temperature and void fraction profiles, the two-phase flooding phenomenon causes a dramatic increase in the frictional pressure drop in this region.

7. Once flooding is established, the vapor generated in the heated zone cannot be easily moved through the downstream region. This leads to pressurization of the heated zone and thus to inlet flow reversal.
8. At inlet flow reversal, a slug type of flow pattern is a good approximation of the observed behavior.

9. For prototypic conditions and a pump coastdown type of flow transient, coolant reentry through the gas plenum region does not occur.

Nomenclature

- AP - pressure drop
- P - pressure
- f - friction factor
- Re - Reynolds Number

Subscripts

- t - total
- i - inlet
- o - outlet

References


OUT-OF-PILE STUDIES IN FRANCE ON SODIUM BOILING

by

J. COSTA, J. LAFAY, R. PLAS(x), JC. ROUSSEAU(x), A. TEYTU
B. MENANT, F. SCHMITT

Centre d'Etudes Nucléaires de Grenoble
Département de Transfert et Conversion d'Energie
Service des Transferts Thermiques

(x) Centre d'Etudes Nucléaires de Saclay
Département de Physique des Réacteurs et de
Mathématiques Appliquées
Service d'Etudes des Réacteurs et de Mathématiques
Appliquées

ABSTRACT

The purpose of this paper is to present the effort being made at the C.E.A. on out-of-pile studies on sodium boiling. Progress concerning the different stages of the programme is given for:
- Development of high heat flux heaters,
- Single and multi-pin boiling experiments in sodium and water,
- Computer codes.

I. INTRODUCTION

In the safety analysis of LMFBR's /1/ several types of cooling disturbances affecting the core are generally considered:
- blockages at the inlet of a sub-assembly (like the FERMI accident),
- local blockages inside the fuel rod bundle, fission gas release,
- pump run-down with simultaneous failure of all shut-down systems.

If such events occur, one has to be able to evaluate what are the damages caused to the fuel, what are the reactivity effects, whether the fault can propagate and whether an early detection of the fault is possible.

Although in normal conditions the coolant is highly subcooled, boiling of sodium is present at an early stage and affects the development of the accident: due to void effects there might be an increase of the core reactivity and the initial conditions of a hypothetical violent sodium-fuel interaction are issued from the boiling stage.

To answer the above questions a good knowledge of the sodium boiling phenomenon is needed.
Boiling of fluid has been studied for many years, but mainly for water reactors (MTR, PWR, BWR, ...) and generally these studies cannot be applied directly to LMFBR situations.

In-pile simulations of loss-of-coolant or reactivity accidents, involving sodium boiling are being carried out \cite{1}, but out-of-pile experiments are useful to get a basic understanding, to validate computer codes and to help in the analysis of in-pile experiments.

The purpose of this paper is to present the effort being made at the C.E.A. on out-of-pile study of sodium boiling.

Up to now the programme has been dealing mainly with the loss-of-coolant accident while the reactivity accident has not yet been studied in detail.

II. PRELIMINARY EXPERIMENTS

The first studies on sodium boiling were of the expulsion type \cite{2}. The test section was an Inconel vertical tube (ID: 6 mm, OD: 10 mm, length: 1200 mm), closed at the bottom and opening into an expansion vessel of 2 liters. The central part of the test section was heated by direct Joule effect.

For each run were determined: the location of the first bubble, the initial superheat, the vapour growth and the expulsion velocity.

The initial superheat values obtained were spread over the range: 20 - 200°C for a saturation pressure range of 0,1 - 1,35 bar abs, a heat flux range of 15 - 130 w/cm² and an initial temperature of 500°C.

An increase of the superheat with time was noticed, but it was not possible to derive precise tendencies concerning the influences of the pressure and the heat input.

The expulsion flow pattern and the voidage of the channel was analysed: after a short expulsion phase, controlled by inertia, of the order of 30 ms, the condensation at the top of the vapour slug is balanced by the evaporation of the superheated liquid, thus leading to a voidage of the heated zone. The proposed mechanism is that liquid droplets are carried along by the vapour and that the rate of evaporation of the superheated zone is controlled by the critical flow rates in the two phase mixture.

Although, by some physical properties, sodium is very different from water, experiments were carried out with water at a low pressure, in parallel with these sodium experiments, to allow a better understanding of that expulsion mechanism.

Water in a Pyrex tube (ID: 6 mm) plugged at the bottom and closed at the top by a metallic diaphragm, was heated up to 150°C by hot oil flowing around the tube. The metallic diaphragm could be melted by an electric discharge and thus it was possible to examine the expansion of the superheated water.

These water experiments confirmed the type of flow pattern of the sodium expulsion: in the first stage, lasting a few milliseconds, the expulsion is controlled by inertia and in the second phase the voidage of the superheated zone is controlled by critical
flow rates in the two phase mixture.

The experimental conditions of these preliminary studies were far from reactor situations with regard to the geometry (a single tube compared to a bundle) and to the working conditions of the fluid (initial static sodium instead of flowing sodium).

Although these preliminary experiments have shown that in spite of their different physical properties, boiling of sodium was governed by the same mechanisms as boiling of water at low pressure, it was considered that it would be very difficult to predict reactor boiling situations on the basis of boiling water experiments only.

Therefore it was decided to start a more realistic programme with the aim to study forced convection sodium boiling in a rod bundle.

III. DEVELOPMENT OF HEATERS

Direct Joule effect was successfully used in the preliminary sodium boiling experiments but this technique gives a poor simulation of a single rod fuel element and is not adequate for bundle geometry.

The indirect heating technique is better but the performances of the available heaters were not sufficient to work under the severe conditions of temperature and heat flux.

To simulate the performances of a fuel pin and to have a heater able to make several tests without damage, the required performances are:
- temperature on the sheath: more than 1000°C
- heat flux: 300 W/cm²
- outer diameter: 6 - 7 mm
- heated length: 400 - 1000 mm

Various techniques have been tested in different laboratories.

Induction heating with high frequency supply is able to give a high heat flux, generated on the surface of the heater owing to the skin effect. This technique is just adequate for use in heating a pipe with internal flow of sodium. It was useful for fundamental experiments but cannot be used for realistic simulation.

Electronic bombardment heating can give very high heat flux and work at high temperature. Great efforts were made, five years ago, to build heaters using this technique. It was not possible to reach a heated length to diameter ratio sufficient to have a good simulation of a fuel rod and to our knowledge no attempt was made to build a rod bundle with this technique.

Another way to build heaters with indirect heating technique was used with success by several laboratories: a graphite resistor insulated from the sheath by boron nitride sleeves. This construction is able to give the required performances and geometry for a good simulation. Its drawbacks are the low voltage and high current due to the very low electrical resistance of the graphite resistor.

Industrial heaters which have nichrome windings embedded in magnesium oxide have been developed by the Watlow Company. The use of
boron nitride as a substitute for magnesia greatly improved the maximum heat flux capability of the heaters. However these heaters could not operate successfully under boiling sodium conditions with nichrome winding and more sophisticated material should be used.

At the Heat Transfer Laboratory, a new type of heater has been developed. The heating wires are parallel to the axis of the pin and arranged in a circle as close as possible to the sheath. (See photo). The two connexions for electric supply are made on the same end of the pin, the sheath being completely electrically insulated from the wires. The high temperature reached in boiling conditions has compelled the use of refractory materials such as tantalum for the sheath, tungsten – Rhenium for the resistor and boron nitride for insulating material.

Clad temperature is measured by thermocouples embedded inside the tube. These thermocouples, 0.25 mm diameter, are in grooves machined into the inner surface of the sheath.

Life time for these heaters depends upon the test conditions but generally they can work several hours under boiling conditions. Some of them reached 1150°C without damage – Thermal shock with rapid increase of power and rapid shut down are well supported by these heaters.

A detailed study of heat transfer between heating wires and sheath has shown that the local heat flux on the surface is maximum in front of the wires. But when the position and the number of heating wires are optimum, the calculation in pure thermal conduction shows that the heat flux is constant within less than 1% error.

High currents cross the heaters to heat the wires by Joule effect and produce a magnetic field which extends out of the sheath in the sodium flow. This magnetic field can interact with sodium, creating electrodynamic forces which can, locally, change the velocity of sodium. A study has shown that, in the case of these heaters, the magnetic field is about 100 gauss near the clad. The influence of this magnetic field on the sodium flow is very small compared to the pressure gradient for velocities greater than 0.5 m/s. It is important to note that, in these heaters two parallel currents of opposite direction reduce the resultant magnetic field; in the same geometry, but with currents in the same direction for the eight wires, the magnetic field would be 250 gauss. As the effect on the sodium velocity increases with the square of the magnetic induction, the disturbance would be more than 6 times greater.

These heaters can easily be assembled in a rod bundle because the electric supply is made on one end only and with a low current. The pins are placed on a flange with a sealed connection base keeping all the thermocouples junctions. The bundle can be completely assembled with all the instrumentation and then fitted to the loop. In some cases the design of connection box allows an independent electric supply for each pin and this is very convenient for test and measurement.

Sodium tightness between the loop and the connection box was first obtained by brazing. But this technique was dangerous for the heaters and did not allow the replacement of a failed pin.
Now a frozen sodium seal has proved to work satisfactorily. Tests on a mock up have permitted to check a calculation code which is now able to calculate sodium temperatures in the seal, taking into account the conduction in three dimensions and the heat flux generated in the leads of the heater.

These heaters were used for the single pin and 7 pin experiments described in chapters IVb and IVc and will be used for the next experiments in a 19 pin bundle.

IV. SODIUM BOILING EXPERIMENTS

IV.a. The CFNa loop (see fig. 2)

Sodium boiling tests in a parameter range of interest for fast reactor safety cannot be envisaged without a forced convection loop. As the stability of boiling is one of the most important features of the problem, great care should be taken to achieve an accurate definition of the boundary conditions at the test section inlet (sodium temperature, outer pressure curve characteristic) and at the test section outlet (cover gas pressure, temperature in the condensing zone...)

The main characteristics of the CFNa loop, built in 1968, according to the above principles are:

Pump: Mechanical, vertical shaft with cover gas, (GUINARD) 3600 rpm, head = 100 m of Na, 10 m$^3$/h

Heat exchanger: finned tubes cooled by air, heat removal capacity: 100 kW

Throttling valves: bellows sealed valves with special seats, (ADAR) pneumatically or electrically controlled.

volume of sodium in the loop: 150 l

main circuitry: stainless steel 316
  inner diameter: 42,2 mm
  outer diameter: 48,6 mm

IV.b. Single pin experiments - main results

As the development of the heaters progressed successfully, it was possible, in 1968, to define a first test section around an electrically heated pin (OD: 6,6 mm - 600 mm long). An attempt was made, as far as possible, to simulate a full subassembly of Phenix: Not only the core region but also the non heated part downstream, including the upper blanket and the exit portion of the subassembly.

The diameters were chosen so that, for the mass flow corresponding to one pin in a subassembly, the velocity of the sodium in the different parts of the test section was the same as in the subassembly.

Although an accident is, by nature, a transient process, the first boiling experiments/3/ were of the steady state type in order to...
proceed from simple situations (steady state—single pin) to more complex and more difficult situations (transient—multi pin).

For given inlet temperature, heat input, outlet pressure, the mass flow was gradually reduced, step by step from normal single phase conditions down to a minimum value corresponding to more than 1000°C on the sheath of the pin. These tests simulate a slow flow reduction due to a blockage at the inlet of a subassembly.

For sodium flowing at a velocity greater than \( \frac{1}{3} \text{m/s} \), and with possible entrained gas from the free surface of the mechanical pump or expansion tank, no significant superheat was noticed.

These results showed also that most of the time the vapour phase was located downstream of the heated zone. In these conditions the geometry of the test section may have an important effect on the hydrodynamics of the boiling process. As an example, for the Phenix geometry, critical flow conditions /4/ may be reached at the test section outlet.

A similar programme was carried out, on a PFR test section, within the framework of a collaboration agreement (UKAEA / CEN) on CFNa.

To investigate the dry out conditions of the pin, special tests were performed and a simple preliminary correlation was derived:

\[
\Phi^\text{D0} = 0.016 \frac{\mu G (1 - X)}{\mu_v} (\frac{X G_d}{\mu_v})^{-1/4}
\]

- \( \Phi^\text{D0} \): heat flux: W/cm² (40 to 120 W/cm²)
- \( G \): mass velocity (13 to 30 g/cm²-s)
- \( G_d \): hydraulic diameter (2 mm)
- \( \mu \): vapour viscosity
- \( X \): vapour quality

For these tests the outlet pressure was \( \approx 1 \text{ bar absolute} \) and the heated length was varied from 400 to 800 mm.

The transient tests /5/ simulate the boiling phenomenon that could occur after a pump run down with simultaneous failure of all shut-down systems. This is the well known "flow excursion" instability, the onset of which is defined by the LEDINEGG criterion. The problem is to predict, in reactor conditions, the voiding rate and the time when the dry out conditions are reached.

A typical result of such a flow excursion is shown on figure 3. Two different stages can be distinguished in this boiling phenomenon:
- a first stage of 10 to 20 s during which the inlet mass flow decreases slowly with a quasi steady state flow pattern.
- a second stage, starting at the same time as the dry out on the pin caracterized by large oscillations of the mass flow (chugging).

To study the governing parameters of this phenomenon a series of tests was performed on different test sections (influence of the geometry) and with different heat flux. They are being analysed.

Recently the result of a similar test has been published by ANL /6/. The test section is about the same as one tested on CFNa.
The general trends are about the same but a direct comparison of this result with our results is not possible because the inlet enthalpy, outlet pressure and heat flux are not the same.

IV.c. Bundle experiments - main results - projects

In parallel with the in-pile SCARABEE 7 pins tests, out-of-pile experiments were performed on the GR 7 tests section in 1973. This test section was a mock up of the in-pile loop and the bundle was built according to the techniques described in chapter III.

Several tests were carried out with 1 or 6 or 7 heated pins under steady or transient conditions and with different outlet geometries. As an example, to show the 3 dimension effects, the different voided zones in the bundle derived from thermocouples information, are presented on figure 4 as a function of the calculated exit quality. In this particular case the heat flux was 47 W/cm² on the 7 pins, the inlet temperature 400°C, the outlet pressure 1.15 bar, and the calculated outlet quality varied from 0 to 6 per cent when the mean flow rate was decreased in five steps from 22 g/s .cm² to 15 g/s.cm².

The analysis of these tests is not completed. Up to now what has been tried is to compare the single pin and the 7 pins results in order to determine how accurate was the simulation of a subassembly by a single pin test section.

Following the successful operation of this 7 pin bundle, experiments on a 19 pin bundle are planned in 1974, to investigate a little more in detail the 3 dimension effects on boiling flow pattern and especially the local flow blockage.

For this particular purpose the CFNa loop has had to be modified, and the new version CFNa II will have a heat removal capacity of 800 kW.

V. WATER EXPERIMENTS

A sodium experiment is expensive because of the special technology required for liquid metals handling. For the velocity measurements, the electromagnetic flowmeters are very simple and powerful tools but for the pressure measurements, the maximum working temperature of the available transducers is far below the boiling temperature of sodium. Furthermore one cannot easily visualize the flow pattern. Therefore, when possible, another fluid is used for the experimental studies.

For single phase isothermal flows, for which a similarity based on the Reynolds number can be used, water is currently used. (see further the experiments in a 19 pin bundle).

For two phase flows no accurate modelling laws exist and the transposition of results is mainly qualitative. Several fluids like freon or water are used.

The particular merits of water as a modelling fluid to simulate sodium in single phase and boiling situations are:
the relative ease of providing adequate and well-known instrumentation,
the easy visualization of liquid and vapour behaviour
the use of available loops built for other purposes.

V. a. Experiments in an annular channel:

An experimental method has been proposed and tested prior to its use on the CFNa loop to determine local critical conditions in a forced convection loop by the measurement of the pressure drop-flow rate curve for two outlet pressures and the same inlet enthalpy and heat input. It was shown that the meeting point of the two characteristics defined critical flow rate conditions.

Critical flow rate of two phase steam water has been measured at the outlet of an annular test section over a very low exit pressure range at the condenser, from 0.2 to 1 bar absolute.

The knowledge of the critical conditions at the exit of the test section is very important because this phenomenon can reduce and prevent the boiling development by autopressurization of the channel but the clad temperature can nevertheless increase even in single phase flow.

Static pressure fields are measured along a heated annular channel of 1 mm gap, upstream and downstream of a local blockage /8/ to determine:

- the hydrodynamic characteristics of the coolant disturbances: pressure drop, pressure recovery and transversal gradient pressure, dimensions of the recirculating zone, damping out of the rotation and head losses of giratory flows;
- the inception of local boiling in the wake of the blockage;

It was found that the lowering of pressure in the recirculating zone does not form a preferential vaporization zone with regard to the boiling at the end of the heated length whatever the blockage rate: 1, 1 or 5

6, 2, 6

The factor which determines the temperature distribution in the wake seems to be the turbulent recirculating flow so that the water experimental results could be extrapolated to sodium flow.

An experimental programme on the flow excursion phenomenon was carried out in parallel with the CFNa experiments /5/.

The transient phenomena due to a flow excursion has been investigated /5/. Three parts have been found in this evolution:

- the apparition of the boiling near the minimum of the pressure drop flow rate curve
- a periodic wall dry-out with a periodic ejection of the coolant,
- a definite calefaction which is reached about 9 seconds after the onset of boiling.

The 2 first steps have also been found in the sodium experiments but the third was not obtained because the power was shut down prior to the final dry out.
V.b. Experiments in rod bundles.

Boiling downstream of a local blockage

Experiments were performed in a helical wire wrapped nineteen rod bundle test section comprising three electrically heated pins and sixteen transparent rods surrounded by a hexagonal duct [9]. Thirteen subchannels were completely blocked by a thin and non porous plate. Thus the blockage was 26% of the total flow area. Hydrodynamic and thermodynamic mechanisms of the cooling disturbance in the wake were observed by high speed cinematographic technics:

The most important phenomenon is the development of a periodic pulsed boiling in the hot subchannel downstream of the blockage which is composed of two phases:

- a phase of liquid heat transfer convection in which the liquid temperature increases from a subcooled state to a sufficiently superheated condition;

- a phase of boiling, characterised by the growth and collapse of the vapour inside the wake. The close observations suggest a considerable importance of the cross and inverse flow in the regulation of the pulsed boiling (see fig. 5).

Single phase liquid pressure field

Preliminary research is being carried out to determine the effect of helical wires spacers on the local pressure for isothermal flow in a nineteen rod bundle [10].

The static pressure profile along the wall of the hexagonal duct is measured:

- along the same peripheral subchannel from the inlet to the outlet rod cluster on 5 helical pitches (see figure 6)
- transversely between different peripheral subchannels of the same hexagonal face.

Differential pressure between static and total probes situated in a triangular subchannel near the center rod of the bundle is also measured with a very small PITOT tube.

These local measurements are needed to adjust some coefficients of the FLICA code in single phase flow. That code will be used to support the analysis of future experimental results in a sodium rod bundle.
VI. COMPUTER CODES

The final objective of the experimental programme presented in this paper is not only to come to a good understanding of the sodium boiling phenomenon but also to provide methods and tools to predict the role of boiling in a reactor situation. Therefore computer codes are developed.

As with the experiments the first approach was made on the single pin geometry.

The first problem consists in the choice of the two phase flow model. As shown by the experimental results presented in chapters IV and V, most of the time the two phase flow is made of many bubbles and can be considered as a homogeneous mixture. This might not be true for the post dry-out situation in the chugging phase at the end of a flow excursion but it is considered that this homogeneous approximation is adequate to describe the two phase mixture in a rod bundle with helical wire that this single channel is intended to simulate.

The FLINA code was written to describe boiling of sodium in a single channel with different cross sections along its length:

- The models used are derived from those of FLICA /11/ and the numerical methods were modified in order to allow the calculation of critical flow rates under steady and transient conditions.
- The main hypothesis are:
  - monodimensional
  - the properties of the fluid are evaluated at the bulk temperature
  - homogeneous model with slip ratio
  - non thermodynamic equilibrium between the vapour and liquid phases

The equations of mass, momentum, energy have been used and a fourth one has been added to describe the disequilibrium between the vapour and the liquid:

\[
\rho_\ell (1 - \alpha) \frac{\partial h_\ell}{\partial t} + G (1 - X) \frac{\partial h_\ell}{\partial z} = \frac{\phi_\ell \times X}{S} + K'(H - h_\ell)(h_{sat} - h_\ell)
\]

were \( \alpha \) is the void fraction, \( X \) the vapour quality, \( \rho_\ell \) the liquid density, \( G \) the mass velocity, \( H \) the bulk enthalpy, \( h_\ell \) the enthalpy of the liquid, \( h_{sat} \) the saturation enthalpy, \( S \) the cross section, \( x \) the heated perimeter.

The values for \( \phi_\ell \), \( K' \) and the slip ratio are fitted to the experimental results.

The thermal inertia of the walls is taken into account and the temperature, mass velocity pressure, void fraction, quality are calculated all along the channel as a function of time.

For some cases the simulation of a subassembly by a single channel is adequate but when the 2D or 3D effects become important a more detailed approach is required and the FLICA II B code (derived from FLICA and FLICA II) /12/ is used.

It is based on the subchannel analysis of a rod bundle where each sub-channel dimension and coupling characteristics are given as input data.
The equations, as for FLINA, are written for an homogeneous fluid assuring a given slip ratio and thermodynamic disequilibrium...
A special model is used to describe the effects of the helical wires. The momentum equation, in the direction perpendicular to the flow, is:

\[ P_i - P_k = \left( \frac{f}{20.5 \cdot 10^7} \right) \cdot \left( \frac{G_{ik}^2 + g_{ik}^2}{\kappa_{ik}} \right)^{1/5} \cdot \frac{1}{\kappa_{ik}} \]

where \( i \) and \( k \) are the indices of the two subchannels on each side of the interface \( ik \), \( P \) is the pressure, \( G \) and \( g \) are the axial and transverse mass velocities, \( \rho \) is the density, \( f \) the friction factor, \( L_{ik} \) and \( D_{ik} \) are dimensions characterising the coupling between the two subchannels.

\[ K_{ik} \] for a bundle with helical wires is given by:

\[ K_{ik} = K'_{ik} \times \left( 1 - \varepsilon \times \frac{\kappa_{ik}}{G_{ik}} \right) \times \cot \alpha \]

\( \varepsilon = \pm 1 \) depending upon the sign of \( K_{ik} \).

The values of \( L_{ik}, D_{ik}, K'_{ik} \) are fitted to experimental results and have different values for triangular or peripheral subchannels.

The code is working in single phase flow and is fitted to water results. In two-phase flow there are difficulties due to the high expansion factor of the sodium vapour.

The code is being modified to improve the description of the flows upstream and downstream of a blockage.

VII. CONCLUSIONS

The main guidelines and the objectives of the out-of-pile programme on sodium boiling carried out in the CEA has been presented in the paper:

The first part of the programme, dealing with a single pin simulation of a subassembly is nearly completed as far as the cooling failures are considered.

The second part of the programme, dealing with a multipin simulation of a subassembly (7 and 19), has been initiated and will be also focussed on the cooling failures and their detection. The GR 19 test section will be used by other laboratories of the CEA to test cooling disturbance detection devices under development.

The study of fast power transients will soon be started in connection with the sodium-fuel interaction study.
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BORON NITRIDE
HEATING WIRES
THERMOCOUPLES Ø 0.25 mm
TANTALUM SHEATH
BORON NITRIDE
HEATING WIRES
THERMOCOUPLES Ø 0.25 mm
TANTALUM SHEATH

SECTION OF A HEATER
FIG. 1

CEA / CENG
DTCE / STT

SECTION OF A HEATER
FIG. 1
Experimental conditions
Loop CFNA = single channel geometry
Test n° 327
W = 40 Kw
TE = 390°C
PA = 1 bar abs

Power shut down
Burn out

Simulation of a pump run down without scram
3 thermocouples in the outlet probe

thermocouples in the helical wires

thermocouples in the walls of the rods

<table>
<thead>
<tr>
<th>CEA / CENG</th>
<th>VOLUME OCCUPIED BY TWO-PHASE MIXTURE IN THE OR7 TEST SECTION</th>
<th>FIG. 4</th>
</tr>
</thead>
<tbody>
<tr>
<td>DTCE / STT</td>
<td>AS A FUNCTION OF THE OUTLET QUALITY</td>
<td></td>
</tr>
</tbody>
</table>
LOCAL BOILING CONFIGURATION DOWNSTREAM OF THE BLOCKAGE

- Pulsed boiling zone
- Cross flow and reverse flow zone
- Steady boiling zone
- Separated flow region

Flow

Direction of wire wrap

camera

CEA/CENG DTCE/STT LOCAL BOILING CONFIGURATION DOWNSTREAM OF THE BLOCKAGE

FIG. 5
G \text{ g/s.cm}^2

\text{STATIC PRESSURE EVOLUTION ALONG A PERIPHERAL RECTANGULAR SUBCHANNEL}
SESSION 12
ACCIDENT ANALYSIS-I
Chairman: W. R. Stratton (LASL)
The theoretical safety analysis of LMFBRs presently concentrates on a mechanistic description of accident sequences which coincide with a complete failure of the reactor safety system. These unprotected accidents may lead to a disassembly of the core. In this paper we discuss the uncertainties in this analysis, which are due to neutronic and non-neutronic methods and data, and the impact of these uncertainties on important safety characteristics of the reactor has been investigated in various sensitivity studies. By this procedure, the most relevant uncertainties are identified. It is found that presently essential experimental verifications on certain hypothesized phenomena in the areas of pin failure, fuel coolant interaction, fuel movement, and fuel slumping are lacking. Therefore we try to define some rules for a conservative safety approach on the basis of our present knowledge. A few experiments are suggested, which are suitable to narrow the range of uncertainties, and allow to reduce the conservatism of the assumptions. In the near future, this experimental effort should have a higher priority than further theoretical speculation.
1. Introduction

At the 1967 Aix-en-Provence Conference on the safety of fast reactors an interesting view into the future situation of analyzing the characteristics of a LMFBR core in an unprotected whole core accident was expressed by Farmer: "I expect there will be continued work on sodium boiling, on Doppler, on improved Bethe-Tait, but again, I do not think that this would change by an order of magnitude the picture that we already have of a dispersed core under accident conditions" /1/. This picture was described by Vendryes in his concluding remarks of the same conference /2/: "...our knowledge is still pretty fragmentary in these fields (of sodium boiling, melt down of the fuel and its possible propagation, energy exchange between vaporized and molten fuel and sodium, etc.)." Bearing in mind that Farmer's view was given for the 5 years 1967-72, it is worthwhile to look at the conclusions drawn by Smidt at the 1972 conference on the "Engineering of Fast Reactors for Safe and Reliable Operation" at Karlsruhe: he called for intensified research work on "fuel behaviour under accident conditions, local fuel and flow failures, sodium fuel interaction, preventive instrumentation and subsequent information processing, realistic early phase core dynamics for the hypothetical core disassembly accident"/3/. There is relatively wide agreement on the needed research topics in 1967 and 1972; to some extent this indicates the relatively slow progress in solving the complex problems involved in unprotected (hypothetical) accidents in fast systems. Farmer's view seems to have not been unrealistic.

Nevertheless in the European countries the construction of LMFBR demonstration plants of about 300 MWe was pursued and today BN350 in the USSR and Phénix in France are in operation, the British PFR just went critical, and in Germany SNR 300 is under construction. We will not debate here, how far this "European Approach" is based on the conviction that one way to assess the complete safety features of a plant is to build it, being convinced that the probability for unprotected accidents is about $10^{-8}$ per reactor year or even less.

In this paper, we try to contribute some further items to a rational assessment of fast reactor safety about 1 1/2 years after the Karlsruhe Conference. We will concentrate on whole core accidents, especially on the discussion of uncertainties in the safety assessment and the impact on important safety characteristics of an LMFBR-demonstration plant of about 300 MWe. The following questions will guide us through this paper:

Q 1. What problems have been solved?
Q 2. What are the key questions at present?
Q 3. What is necessary in order to find the answers, or at least narrow the uncertainty ranges?

This investigation will be performed before the background of the situation in Germany. It should be kept in mind that such an undertaking is not free from personal judgment, and the paper cannot claim to give the German view on fast reactor safety. Furthermore we cannot aim here to give a complete critical review, e.g. with regard to point Q 3 in the above list.

2. Safety Analysis Methods at GfK Karlsruhe

The development of codes describing the various events in unprotected accidents was strongly influenced by the work performed in the US, mainly at Argonne National Laboratory. We highly appreciate the pioneering research at ANL. In this section we restrict ourselves to a few comments, characterizing the various codes; a more comprehensive presentation is given in /4/. The basic calculational tool is the code system CAPRI/KADIS, which is similar to the combination SAS II/VENUS at ANL. CAPRI deals with the predisassembly
phase of an unprotected accident: This code uses point kinetics and first order perturbation theory to calculate the reactivity feedback; up to 30 cooling channels can be treated, each channel representing the central pin cavity, the inner and outer grain zones, gap, cladding, coolant and subassembly structure. The module BLOW3/5 treats sodium boiling, ejection and reentry phenomena; it simulates the two phase flow by a sequence of liquid slugs and bubbles; it has been successfully compared to the results of out-of-pile experiments. Elastic plastic deformation of the cladding is accounted for/6/, as are the effects of fuel coolant interaction (FCI) and fuel slumping after clad melting. The FCI module is identical with the model of Cho and Wright/7/ in the first phase (liquid sodium only), while for the subsequent evaporation phase the Caldarola model/8/ is used.

The slumping module/9/ assumes that the molten fuel slumps into the intact geometry of the lower part of the core, and simultaneously the intact upper parts of a subassembly fall towards the midplane of the core with a prescribed friction parameter. The disassembly code KADIS/10/ has been developed out of an earlier version of the ANL-VENUS code and is at present in its physical content identical with VENUS II/11/.

Because some later results will be based on a BELGONUCLEAIRE predisassembly module, called CARMEN II/12/, a brief account of this calculational tool is also given here. CARMEN II is a point kinetics code with multiregion heat transfer and temperature dependent properties, monitoring fuel pin failure, coolant boiling and ejection. The voiding pattern due to FCI is given by input.

This safety code system is supplemented by two two-dimensional space dependent codes: RADYWAR II, using an improved synthesis scheme/13/, and KINTIC II/14/, based on the improved quasistatic approach.

For the investigation of the FCI at Karlsruhe two codes have been developed:

The Caldarola model differs from the Cho and Wright model mainly in that it theoretically determines the thickness of the vapor film surrounding fuel particles. Recently it has been extended to include a particle size distribution and variable masses of fuel and sodium/15/. The second code, BRENDY II/16/, is based on more general assumptions. In this one-dimensional Lagrangian hydrodynamics code the sodium is treated as a heat conducting compressible fluid. It is interesting to note that this model gives less conservative results than the ANL parametric model. Work is in progress to include the description of a non-uniform mixing zone (optionally containing gas) and variable masses.

3. Uncertainties of Neutronic Quantities involved in Safety Analysis

An assessment of the target accuracies and calculational uncertainties for physics parameters of a 300 MWe plant has been made in 1972/73/17/. For the quantities of interest in safety analysis the following results have been obtained for the uncertainties:

Doppler Coefficient: \( \pm 20\% \)

Sodium Void Coefficient: \( \pm 20-30\% \).

It should be mentioned that in obtaining these figures, the best theoretical methods and data available at Karlsruhe have been used to analyse about 30 fast critical assemblies, and the uncertainties resulting from the extrapolation to power systems of about 300 MWe are taken into account.

It should be noted that:

a) normally somewhat less sophistication is used in describing the neutronic properties in reactor transients than is done in static criticality calculations (e.g. group number, streaming effects).

b) material movements in the predisassembly (e.g. Na ejection and fuel sweep out) and disassembly phase at least require a check whether space dependent kinetic models have to be used to determine the appropriate reactivity feedback.

As an example we demonstrate the influence of group collapsing schemes and of
neutron streaming effects on the accuracy of the sodium-void reactivity. The usual procedure of group collapsing preserves only the reaction rates, but the reactivity coefficients determined in first order perturbation, may be erroneous. Condensation from 26 to 6 energy groups yields the result that the values of the local Na-void coefficients are reduced, in the central core regions between 15 and 30%. Because the negative contributions to the void effect become more negative, this procedure gives far too optimistic reactivity responses. In Figure 1 these differences in the void reactivity coefficients are shown. Sufficiently accurate results are obtained by using the bilinear group collapsing scheme /18/.

Table 1 contains the heterogeneity effect, due to neutron streaming, on the sodium void reactivity coefficient. Some of the selected voided regions in a simplified 2d geometry represent typical void patterns during flow coast down accidents. The numerical results are based on a theory developed in /19/.

Table 1: Comparison of homogeneous and heterogeneous calculations for Na-void reactivity.

<table>
<thead>
<tr>
<th>Change in criticality</th>
<th>CASE 1</th>
<th>CASE 2</th>
<th>CASE 3</th>
<th>CASE 4</th>
<th>CASE 5</th>
<th>CASE 6</th>
<th>CASE 7</th>
</tr>
</thead>
<tbody>
<tr>
<td>(Δk)_{Hom} [%]</td>
<td>+13.9</td>
<td>+10.7</td>
<td>+9.1</td>
<td>+6.8</td>
<td>+1.8</td>
<td>+3.7</td>
<td>-3.9</td>
</tr>
<tr>
<td>(Δk)_{Het} [%]</td>
<td>+13.4</td>
<td>+10.0</td>
<td>+7.9</td>
<td>+5.1</td>
<td>-1.2</td>
<td>+1.9</td>
<td>-8.1</td>
</tr>
<tr>
<td>(Δk)<em>{Hom}-(Δk)</em>{Het} [%]</td>
<td>0.5</td>
<td>0.7</td>
<td>1.2</td>
<td>1.7</td>
<td>3.0</td>
<td>1.8</td>
<td>4.2</td>
</tr>
</tbody>
</table>

Voided regions in each case:
Case 1: Approx. 2/3 of 1st core zone (about maximum void reactivity)
Case 2: 1st core zone
Case 3: 1st core zone and half of upper blanket
Case 4: 1st core zone and both adjacent axial blankets
Case 5: 1st and 2nd core zones
Case 6: Both core zones and half of both adjacent axial blankets with the last subassembly row in each core zone unvoided
Case 7: 1st and 2nd core zones and axial blankets

In this context two points seem to be important:
- a) The streaming effect improves the situation only if relatively large fractions of the reactor zones are voided.
- b) The results are conservative, because no control- and shut down rods are considered.

Since the streaming effect increases the leakage of neutrons out of the voided regions in the radial direction too, a larger fraction of neutrons will penetrate into non-voided regions containing absorbing materials. This effect is under investigation now.

In order to have a sound basis for a thorough comparison of point- and space-dependent kinetic models in fast reactor safety analysis, Terney /20/ investigated various group collapsing and energy synthesis schemes with an improved version of the 1-dimensional RAUMZEIT code with simple feedback. With the already mentioned bilinear collapsing procedure, the power evolution was represented with sufficient accuracy. In a disassembly calculation, the omission of flux shape alterations in the point kinetics approximation can cause a major error for the value of released thermal energy, leading to optimistic estimates, as has been shown by Smith and Ott /21/. In a similar way we calculated the power evolution of a SEFOR superprompt critical overpower transient by using the initial flux shape for reactivity determination. In Fig. 2 the result is compared with that of a two-dimensional quasistatic calculation, which gives satisfactory agreement with experiment.

These uncertainties, which are related to the kinetic model used, do not represent a fundamental difficulty; the tools are available and a comprehensive in-
Table 2: Influence of uncertainties on safety quantities after nuclear shut down in the disassembly phase

<table>
<thead>
<tr>
<th>CASE</th>
<th>BASE</th>
<th>CASE 2</th>
<th>CASE 3</th>
<th>CASE 4</th>
<th>CASE 5</th>
<th>CASE 6</th>
<th>CASE 7</th>
</tr>
</thead>
<tbody>
<tr>
<td>BASE</td>
<td>CARMEN/KADIS calculation with Mark1-SNR-300 data /22/</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>CASE 1</td>
<td>1.087</td>
<td>1.130</td>
<td>1.062</td>
<td>1.156</td>
<td>1.106</td>
<td>1.087</td>
<td>1.028</td>
</tr>
<tr>
<td>CASE 2</td>
<td>58.2</td>
<td>60.0</td>
<td>61.1</td>
<td>97.0</td>
<td>78.4</td>
<td>58.2</td>
<td>26.0</td>
</tr>
<tr>
<td>CASE 3</td>
<td>0.6356</td>
<td>0.8300</td>
<td>0.4337</td>
<td>0.9707</td>
<td>0.8500</td>
<td>0.6356</td>
<td>0.3125</td>
</tr>
<tr>
<td>KADIS results:</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Initial reactivity [g]</td>
<td>3.84</td>
<td>2.88</td>
<td>4.86</td>
<td>2.57</td>
<td>3.33</td>
<td>2.48</td>
<td>6.75</td>
</tr>
<tr>
<td>Initial ramp [g/sec]</td>
<td>1.31</td>
<td>2.38</td>
<td>0.827</td>
<td>2.86</td>
<td>1.75</td>
<td>1.16</td>
<td>0.361</td>
</tr>
<tr>
<td>Initial power [10^6MW]</td>
<td>2676</td>
<td>3565</td>
<td>2141</td>
<td>3639</td>
<td>3811</td>
<td>1366</td>
<td>1770</td>
</tr>
<tr>
<td>Duration of disassembly [msec]</td>
<td>4324</td>
<td>4745</td>
<td>3959</td>
<td>4754</td>
<td>4848</td>
<td>3255</td>
<td>1352</td>
</tr>
<tr>
<td>Energy of molten fuel [MJ]</td>
<td>3814</td>
<td>4117</td>
<td>3635</td>
<td>4149</td>
<td>4196</td>
<td>3359</td>
<td>3410</td>
</tr>
<tr>
<td>Mass of molten fuel [kg]</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Av. temperature of molten fuel [K]</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

Contents of TABLE 2:

Case 1 (Base case): CARMEN/KADIS calculation with Mark1-SNR-300 data /22/
Case 2: The Doppler coefficients in each region are multiplied by the factor 0.7
Case 3: The Doppler coefficients in each region are multiplied by the factor 1.3
Case 4: The radial distribution of the sodium worth is flattened, keeping the central value constant
Case 5: The radial power shape factor is changed to 1.169 (1.368 in the base case!)
Case 6: FCI option in the disassembly phase is used (fuel particle radius: 0.015 cm, mixing time constant: 3 msec)
Case 7: Na-slug ejection velocity reduced by factor of 3
vestigation is underway at Karlsruhe.

The impact of uncertainties in the Doppler and sodium void reactivity on essential safety quantities after nuclear shut down in the disassembly phase is given in Table 2. An unprotected overpower transient in the Mark 1 Core of SNR 300/22/ was initiated by a ramp rate of 5 $/sec.

From Table 2 it can be concluded that the uncertainty in the Doppler coefficient yields about the same uncertainty in released energy, while the dependence on the reactivity ramp rate follows roughly a square root law, if the reactor states at the transition to disassembly are not too different and the excursion is shut down after the first power peak.

Case 5 in Table 2 indicates the trend in released energy for flat power distributions. Especially commercial size reactors between 1000 and 2000 MWe with 3 or even 4 core zones are economically attractive. But this power flattening is, based on the present codes, accompanied by an appreciable increase in thermal energy yield. The increase is directly proportional to the fuel inventory for idealized flat power distributions and otherwise similar core characteristics.

For convenience the effects of non-neutronic parameters on energy yield have been included in Table 2; they will be discussed in section 4.2.

4. Uncertainties of Non-Neutronic Parameters and Models

In the field of a mechanistic description of an unprotected excursion the non-neutronic uncertainties have a different character from what was mentioned in chapter 3, because some of them cannot simply be expressed as a variation of numbers.

The present situation is that:
- we may not be aware of all contributing mechanisms;
- a full understanding of already identified mechanisms is lacking;
- one can not necessarily rely on the actual occurrence of already hypothesized mechanisms.

The first category of uncertainties lies in the extrapolation from out-of-pile or inpile experiments (e.g. in thermal transient reactors like TREAT) to the transient behaviour of a whole fast reactor core. Some of the experimental results on 1 to 7 pin bundles (e.g. in the TREAT facility) may suffer from the relatively short pin length, and the cold loop wall, where for instance rapid Na condensation and even fuel solidification takes place presumably quite differently from in a reactor situation.

The second category of uncertainties contains phenomena which already can be described by codes without being sure that the basic physical model is applicable to the accident situation. These phenomena include pin failure, ejection of molten fuel from the failed pin, fuel coolant interaction (FCI) and fuel lumping.

By now, FCI definitely is the best understood phenomenon among these. There is a whole variety of models which (with more or less accuracy depending on the computational effort) describe rather well the course of events after the establishment of certain starting conditions: i.e. fuel and sodium are uniformly mixed throughout some axial region of a subassembly. But at present it is doubtful if such starting conditions will occur in an accident situation.

These doubts emerge from first ideas on the mode of fuel expulsion from failed pins and growing knowledge on the fragmentation process. An (possibly minor) uncertainty exists in the interaction of the pressurized sodium with the surrounding structures and the plenum.

The third category of uncertainties is constituted by the fuel sweep out. It can hardly be imagined that a FCI as described by the present models can take place without a fast movement of the dispersed fuel with the sodium, leading to a fast shut down mechanism. To our knowledge neither a large-scale FCI nor fast fuel sweep out have been proven experimentally so far. In the following subsections we will discuss in more detail the present situation and relevance of
4.1 Fuel Pin Failure

The importance of a relatively accurate prediction of fuel pin failure in an overpower transient emerges from the sensitivity of subsequent accident sequences on this process, in particular,
- fuel movement in the central pin cavity upwards if the pin fails in the upper part of the core, leading to a negative reactivity feedback contribution,
- mode and amount of fuel expulsion into the coolant channel, initiating FCI and fuel sweep out,
- mode and amount of fission gas release during fuel melting, leading possibly to a strong and fast disassembly mechanism.

The experimental evidence on mode of pin failure is rather poor or even lacking. One of the main reasons is that post mortem inspections do not provide reliable answers, except on the location of failure, and "on line" inspection for inpile experiments up to now are difficult or impossible. Furthermore in experimental investigations no reliable results can be found on whether the retained fission gas causes the fuel to foam during melting and expulsion or whether an effective separation of gas and fuel occurs.

A point of minor importance is that fuel pin temperatures in the range of 3000 K cannot be determined (neither by calculation nor by measurement) with a higher degree of accuracy than up to ± 10%. However, in the analysis of the predisassembly phase, the pin failure threshold is often monitored by a mean fuel temperature, clad temperature or area of melted fuel in the pin. This threshold then determines the onset of FCI and simultaneously the release of fission gas. In an overpower transient a higher threshold has the effect that a larger amount of molten fuel interacts with sodium. In that case voiding of channels will be faster and the disassembly will start earlier (compared to the time of first pin failure).

Recently we included into KADIS a routine to describe the pressure build up due to vaporized fission products /23/, considering fission products with high relative yield and high vapor pressure. The parametric studies show a reduction of thermal energy yield in all cases, compared to the case without accounting for fission gas release (see Table 3). However: a reliable consideration of the effects of fission gas release must come along with better understanding of and a corresponding model for the dynamics of fuel and gas behaviour in the transient, primarily in the predisassembly phase.

### Table 3: Influence of fission gas release in the disassembly phase

<table>
<thead>
<tr>
<th>Result</th>
<th>Burn up 3%</th>
<th>Burn up 0%</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Rare gases</td>
<td>ANL-equation of state</td>
</tr>
<tr>
<td>Total energy released [MJ]</td>
<td>100</td>
<td>760</td>
</tr>
<tr>
<td>Energy in molten fuel [MJ]</td>
<td>0.4</td>
<td>84</td>
</tr>
<tr>
<td>Mass of molten fuel [kg]</td>
<td>1.5</td>
<td>298</td>
</tr>
<tr>
<td>Maximum fuel temperature [K]</td>
<td>3040</td>
<td>3040</td>
</tr>
<tr>
<td>Maximum fuel melt fraction</td>
<td>4%</td>
<td>86%</td>
</tr>
</tbody>
</table>

In the first case the rare gases (Kr and Xe) are included to build up the fission gas pressure; in the second case only the other fission products contribute to the pressure build up. In these two cases the pressures produced by fis-
sion products are released in each mesh cell proportional to the melt fraction. The results calculated with the ANL-EOS /11/ are given for comparison. All cases have been calculated with a 50 $/sec ramp rate, initiating the disassembly.

4.2 Fuel Coolant Interaction

Experimental results with UO$_2$ and sodium show that UO$_2$ fragments extensively, but no explosive interaction has occurred up to now except in the case where a few grams of sodium were injected into molten UO$_2$ /24/. On the other hand explosions are easily produced with molten metals and water /25/. The explosion mechanisms are different in both classes of experiments. They can be identified — and at the same time excluded for a reactor accident situation — by use of the spontaneous nucleation hypothesis of Fauske /26/.

The basis of explosions in the metal-water system is the fact that the contact temperature in practically all cases is far above the stability limit of the water at which spontaneous nucleation starts. Thus hot metal entering the water is covered by a 'stable' vapour film which enables the metal to develop a large interface with the water without any violence as is connected with nucleate boiling, and without large heat losses. If now the vapour film is caused to collapse, a surface explosion occurs, i.e. a volume of water, which is given by the contacted metal surface times some penetration depth of the heat, is raised to a mean temperature a little below the contact temperature but still far above the stability limit so that it evaporates suddenly with considerable vapor pressure. If the triggering mechanism causing the film collapse or fast subsequent processes produce new metal surface, the violence of the explosion obviously is increased.

In Zyszkowski's experiments /27/ in 3 cases out of roughly 200 tests a copper droplet exploded while it was at rest at the bottom of the water basin. The transient temperature measurements indicate that the explosions followed direct contact to the liquid water. From the high-speed movie it can be concluded that some mechanism originated in the particle, triggered the film collapse.

When the initial metal temperature is so low that the contact temperature is below the stability limit, transition boiling and/or nucleate boiling may contribute to fragmentation of the metal but no explosive vaporization occurs. This condition also is met in the UO$_2$-Na case. Fauske and co-workers /26/ have derived that the bulk temperature of the UO$_2$ must exceed 5000 K to 7000 K in order that the contact temperature exceeds the stability limit of the sodium. At lower temperatures the UO$_2$-Na system lacks the insulating vapor film which is the basis of the above described vapor explosion mechanism. Rather boiling will occur, leading to rapid cooling of the fuel. These processes are likely to cause fragmentation of the fuel droplets according to the violent boiling hypothesis and/or by thermal stresses induced in the droplets near solidification.

With respect to the only two observed explosions of the UO$_2$-Na system Fauske's explanation is /26/:

"...it is not surprising to observe consistent small-mass vapor explosions when liquid sodium is injected into a bath of molten UO$_2$... the lack of nucleation sites in the liquid-liquid-like system results in overheating of the liquid sodium until spontaneous nucleation occurs. When the superheat limit is reached, vaporization is rapid enough to produce shock waves.

On the other hand, in LMFBR environment, an ample supply of nucleation sites is generally available ... Therefore, molten UO$_2$ encountering liquid sodium will generally lead to nucleate boiling ...".

In this way the second explosion mechanism identifiable in the experiments done so far (the reason of the explosion being the lack of nucleation sites in a liquid-liquid system) can also be very probably excluded from occurring in reactor accidents. But still there remains a further mechanism which may lead to explosive fuel-sodium interaction. This mechanism is characterized by a pressurization of the heated sodium during the heating process which inhibits nucleation. When at some time the pressure drops back to the ambient pressure again there exists a large amount of sodium which is superheated with respect to the
saturation temperature for the ambient pressure. A possible process would be to heat the liquid sodium so quickly that its thermal expansion cannot be absorbed by the system and for a limited time produces high pressures. This will happen only if a situation as envisaged in the present FCI models appears: Many small fuel particles are dispersed rapidly in some volume of liquid sodium, so that the heating surface is much greater than the flow area through which the pressure is discharged. Since the pressure build up in such a situation is a matter of around 100 usec, already very small time-lags in the formation of the mixing zone have a great influence on the course of the events. Therefore the problems of fragmentation and mixing zone formation have become the key questions of FCI. In the theoretical models these processes are characterized by two parameters: the fuel particle radius and the mixing time. The latter in many models merely is a parameter without direct physical meaning (therefore called mixing time constant) which makes it possible to examine the effects of a finite rate of fragmentation and mixing. Only very recent models developed by Reynolds et.al. /28/ and Caldarola /29/ describe a true variation of the interacting masses during the mixing time. It has been shown in /28/ that the work done by FCI in the range of extremely small particles is almost independent of their size. Already with 100 µm diameter particles more than 90% of the asymptotic work (corresponding to zero particle size) is obtained. But larger diameters reduce the total work and - which seems to be more important - slow down the process. This is demonstrated by Table 4 which gives results from calculations with BRENHY II. The description of the test case may be found in /16/.

Table 4: Influence of particle diameter on FCI

<table>
<thead>
<tr>
<th>Particle diameter [µm]</th>
<th>234</th>
<th>600</th>
<th>1200</th>
</tr>
</thead>
<tbody>
<tr>
<td>Peak pressure [bar]</td>
<td>2517</td>
<td>1265</td>
<td>696</td>
</tr>
<tr>
<td>Work done during the acoustic period [J] (per g UO₂)</td>
<td>12.33</td>
<td>4.13</td>
<td>1.12</td>
</tr>
<tr>
<td>Initial velocity of the sodium slug after the acoustic period [cm/sec]</td>
<td>13 135</td>
<td>7600</td>
<td>3959</td>
</tr>
<tr>
<td>Velocity of the slug when it leaves the core [cm/sec]</td>
<td>14 610</td>
<td>8860</td>
<td>4090</td>
</tr>
<tr>
<td>Time of this event [msec]</td>
<td>3.68 (3.67)</td>
<td>5.29 (5.23)</td>
<td>9.21 (7.70)</td>
</tr>
<tr>
<td>Work done until the sodium slug has covered 50 cm [J] (per g UO₂)</td>
<td>17.32 (17.60)</td>
<td>6.77 (7.83)</td>
<td>2.66 (3.58)</td>
</tr>
</tbody>
</table>

The table gives the peak pressure and the work done during the acoustic period, the initial velocity of the unheated sodium slug at the beginning of the inertial phase, slug velocity and time when the slug's rear surface leaves the core and the work done until the slug has travelled 50 cm. With the last three items the values given in brackets originate from calculations in which the isolating effect of the sodium vapor has been neglected. (The very last row has been depicted in Figure 3). Slowing down the FCI has two opposite effects on the energy yield. In the predisassembly phase it results in slower voiding (see Table 4). And small voiding ramps tend to decrease the final energy yield. As an example we parametrically reduced the ejection velocity of the sodium in our test case by a factor three. This reduced the transition ramp from 58 $$/sec to 26 $$/sec and the energy yield to about 50%, see Table 2.

On the other hand, in the disassembly phase, when heterogeneous mutual displacements of fuel and sodium are no longer taken into account, slow pressure build up means late disassembly. Thus starting with the same transition ramp a particle diameter of 500 µm gives 200 MJ (roughly 10%) more thermal energy than a diameter of 300 µm /30/.
Arguments similar to that above can be made with respect to the mixing time, the main difference being that within the reasonable parameter range the dependence of the results on the latter is weaker. This is demonstrated in Figure 3 with the work done by the FCI until the sodium column has moved 50 cm. The results with different mixing times are due to Cho et al. [31] (squares) and Reynolds et al. [28] (circles). In the latter case the zero mixing time has been used as the basis (reduction factor equal one), since no other value was available and obviously there is no change of the work when 0.1 msec is used as mixing time instead of 0 msec.

But there is one point that must be kept in mind, especially in connection with predisassembly calculations: Jacobs [16] has shown (and it can also be seen from Table 4) that the voiding history is controlled mainly by the pressure peak during the acoustic phase, supposing there is a peak of order 1000 bar. (In contrast with that, the work done during the voiding process also depends strongly on the heating rate at that time). This will be the case in most instance with zero mixing time. Now Caldarola [29] has shown that already 1 msec mixing time reduces the peak pressure by a factor of almost 5. Thus already such small mixing times considerably reduce the reactivity insertion due to voiding. In disassembly calculations we have found (starting always with the same transition ramp) that taking into account FCI with 10 msec mixing time reduces the energy yield only by 10%. With 3 msec mixing time the reduction amounts to 40%. (The particle diameter was assumed to be 300 μm.)

In connection with the consequences of FCI, the aspect of incoherent onset of FCI in subassemblies with different burnup will have considerable influence on the associated voiding reactivity pattern and therefore also on the production of thermal energy. The cylindrical ring coherence of our models is conservative. A cycle strategy, which is based on three types of subassemblies with different burnup, would therefore result in a lower void reactivity ramp rate than in the present cylindrical ring treatment together with a corresponding lower reactivity level. If according to the preceding discussion explosive vaporization (due to pressurization) can be considered as a very unlikely process, violent boiling in only a small part of the subassemblies would yield only moderate reactivity changes. Fuel sweep out finally would make this accident sequence a relatively unimportant one, if the release of fission gas into the channel does not take over the role of a fast-acting voiding mechanism.

4.3 Fuel Movement

At the beginning of chapter 4 the importance of the fuel release mode and the fuel sweep out was already mentioned. In spite of the fact that the neutron hodoscope at the TREAT facility has quite well detected slow eruptions of molten fuel in flow coast down experiments [32], to our knowledge it has not observed fast fuel movement, neither in the central pin cavity nor in the coolant channel. We do not know whether this can be explained by the characteristics of the hodoscope or by the smallness of the amount of fuel dispersed in the channel. In any case it is not sufficient to find some fuel outside the test section in post mortem inspections, since an effective shut down mechanism is provided only if fast fuel movement occurs.

A consistent treatment of fuel movement in the channel seems to be the use of the usual FCI models with a mixing zone concept and consideration of hydraulic fuel sweep out. But again the questions are, where the mixing zone initially is situated, and how much fuel actually reacts with the coolant and reliably is swept out. The effect of such a consideration on the released excursion energy has been demonstrated by various parametric studies [33,34]. Obviously the results depend on the choice of the above mentioned open parameters. In [33] using a mixing zone at the core midplane the energy yield is reduced by a factor 2 to 4.

4.4 Fuel Slumping in Pump-Coast-Down Accidents

In an unprotected pump coast down accident the first important feature is the
boiling and dryout sequence. Up to now boiling phenomena have been treated theo-
retically only in a one-dimensional geometry. As has been demonstrated by
Chawla and Fauske /35/, there exists a remarkable temperature gradient across a
subassembly which is enhanced for reduced flow conditions. This means that the
subchannels near the subassembly wall are below the saturation temperature and
therefore boiling in these channels will occur with an appreciable time delay.
This boiling incoherence in a subassembly will modify the void reactivity pat-
tern and this influences the subsequent course of events.
To the above mentioned incoherence one must add the incoherence of fuel slump-
ing in subassemblies with different burnup. To our present understanding four
major effects may simultaneously occur during the fuel slumping phase:
a) After dryout the clad will melt and under the gravity and Na vapor drag
forces the clad will be moved towards the blanket regions. Besides the associ-
ated positive reactivity effect (which possibly will be small due to neutron
streaming), this clad motion may lead to a partial or complete blockage of the
coolant channels. This was indicated in the experiments of the L series in TREAT
e.g. /32/.
b) The melting fuel releases the retained fission gases and/or fuel jets may be
driven out of the pin to relieve the pressure.
c) Fuel will move down the central pin cavity and fill it up. This will lead to
a decrease in reactivity at the beginning of 'downflow', but later an increase
of reactivity can follow, when the central pin cavity has been sufficiently
filled.
d) In irradiated pins the fission gas pressure inside may burst the pins rela-
tively early, while in fresh fuel melting can be the dominant mode.
e) After the loss of mechanical pin stability the upper, no longer supported
parts of the fuel may move towards the midplane of the system, thus increasing
the reactivity.
At present experimental results are insufficient to draw satisfactory and reli-
able conclusions on the modes mentioned above, so that theoretical modelling
must include the various events in parametrized form, the true importances be-
ing unknown. The main interest in the events a) – d) is related to the accom-
panying reactivity effects. One of the most essential quantities in current
models is the parameter on which depends the motion of the upper parts of the
fuel pins. We have found by sensitivity analysis that with a completely blocked
upper region, a pump coast-down accident might not reach a disassembly condition,
while for the assumption of a free or hindered fall, transition to core disas-
sembly takes place. Thus the uncertainty in the behavior of the core after loss
of mechanical pin stability has large consequences. The radial incoherence of
slumping in a subassembly very probably will result in reduced reactivity in-
sertions so that a more realistic description may yield less conservatism and
milder consequences.
4.5 Recriticality
Besides the problem of decay heat removal, the possibility of recriticality of
the distorted core after disassembly is of great importance. Due to the high
enrichment of fissionable material in fast demonstration plants of about 300 MWe,
recriticality can be achieved in principle either in the core itself, or in the
tank, or in an external core catcher below the tank. The uncertainties are rath-
er large in this area, since there is only poor experimental support from some
out-of-pile investigations, especially concerning thermodynamic behaviour of
molten core materials. Because of this situation, the theoretical procedure can
only give rough and plausible estimates on possible configurations, accident
sequences and related energy yields. Of course in such a situation one must keep
the analysis strictly conservative; nevertheless one should try to avoid overly
pessimistic assumptions.
The consequences of an unprotected power excursion of a fast prototype reactor
are unlikely to result in a wide dispersion of hot core material; more likely,
most of the molten fuel and steel will remain somewhere at the location of the core just after nuclear shut down. If this is assumed, the compact molten or granulated core material can and probably will go critical with some moderate reactivity insertion rate; the ramp rate depends strongly on the assumptions made with respect to the dynamics of molten material on the way to criticality and superprompt criticality. If the excursions are mild, one can expect also a mild energy yield and possibly the removal of some fuel out of the core region. This process can be repeated, probably several times. We will not discuss in detail the melting of the fuel through the grid plate and also through the tank, where again recriticality cannot be excluded in the absence of preventive measures /36/. The addition of neutron absorbing materials to the molten or granulated material in the bottom of the tank will reduce the chance for a further excursion. In principle, low reactivity ramp rate excursions in the tank or core catcher operate as a dispersion mechanism for the molten material and therefore should be tolerated. However, it could not be demonstrated so far that only such mild excursions can occur. In fact, if one makes the pessimistic assumption that two subcritical blocks approach under gravity, and that the blocks consist of low density granulate, one can calculate excursion energies which probably cannot be contained.

Thus, at present, one can only carry out sensitivity studies to assess the influence of different parameters. One can expect that the growing knowledge on the dynamic behavior of molten materials will help to narrow the uncertainties to an acceptable range.

5. **Conservative Safety Analysis and Efforts needed to improve the Prediction of LMFBR Safety Characteristics in Whole Core Accident Situations.**

In the preceding sections we have identified the uncertainties of neutronic and non-neutronic parameters and models, and we also studied the impact of these uncertainties on important safety quantities. Furthermore the foregoing discussion has shown that the present theoretical and experimental knowledge does requires the use of conservative assumptions in the theoretical description of some accident sequences. In this chapter we indicate a possible procedure for a conservative safety analysis. However, this procedure may lead to difficulties in containing the consequences of unprotected accidents in a reactor system, especially if commercial size reactors are considered. Next, we try to specify the necessary theoretical and experimental effort in order to reduce the present uncertainties. As indicated in this paper, such an investigation very probably would lead to the result that the use of less conservative assumptions is justified.

5.1 **Conservative Safety Analysis**

Both for an unprotected over-power transient and a pump coast down accident a conservative safety approach is outlined.

### I. Overpower Transient:

a) Assume the location of pin failure at the midplane of the reactor core to locate the FCI mixing zone around midplane (we exclude explicitly initial pin failure below the midplane).

b) Assume that after pin failure 50% of the fuel in the failure region interacts with the coolant. This estimated figure is in the range of plausible assumptions, conservative except for fresh fuel.

c) Use a mixing zone model for FCI with 200 μm particle diameter and 3 msec mixing time constant.
d) Do not consider fuel movement.

e) Do not consider incoherence effects due to different burnup of subassemblies.

II. Pump Coast Down

a) Do not assume incoherent boiling due to temperature gradients across the subassembly.

b) Assume free or weakly hindered fall of the upper subassembly structure.

These prescriptions yield conservative results when using the safety codes discussed in chapter 2. Improvements are mandatory as long as unprotected accidents are considered as a design basis.

5.2 Effort needed to improve the Prediction of LMFBR's Safety Characteristics

The following discussion emphasizes mainly the need for a reliable experimental verification of some of the important processes in an accident sequence. Theoretical support and guidance is necessary, but is not listed explicitly.

I. Overpower Transient

1. One of the most important questions is, whether it is possible to confirm by experiment that fast fuel movement inside the pin and in the coolant channel in all cases of recognized pin failure during an overpower transient has the reliable potential to reduce the consequences of this accident to a tolerable level or even shut the reactor down before a Bethe Tait type disassembly occurs. Though experiments in TREAT or later CABRI /37/ suffer from the fact that not all experimental results are directly applicable to large fast power reactors, the investigation of the fundamental processes of fuel movement and the location of initial pin failure will reduce the present uncertainties. However, it is insufficient just to perform post mortem inspection on the pin or bundle: rather, on-line inspection is required. This in turn means for instance an accurate and reliable operation of an appropriate detection device, e.g. a hodoscope; further research and design work is necessary in this area.

2. If this development fails to provide the necessary information, because fast fuel movement does not occur or one cannot rely on its occurrence, the next important experiment would be to determine time and mode of fuel release into the coolant channel, dependent on rod power, burnup and the rate of temperature increase. This has to go along with a careful examination of the axial distribution of fission gases and of the actual release rate for retained fission gases especially in the equiaxed grain region during melting. This experiment should clarify whether the fuel is being dispersed into the channel in the way assumed in most of the FCI models, or whether it is squeezed through the ruptured can with moderate velocity; as a mixture with the fission gas (froth), or mainly separated from the gas. This experiment would help to determine the starting conditions for a subsequent FCI. It must be made in pile because otherwise the results might be spoilt by the heating device. Thus again one of the in-pile test facilities with a satisfactorily functioning detection device could provide the answers. It does not seem necessary that the experiment be performed with Na as coolant (experiments with gas environment possibly are easier to perform). This experiment should clarify also the possibility of fuel freezing.

3. Equally important would be an experiment, which convincingly shows that for all interesting burnup situations the thermal expansion of fuel in a transient is a reliable mode to reduce reactivity.
4. The next important task would be to show that under reactor accident conditions Na superheat can be excluded, such that the entrainment of Na into fuel only leads to boiling and does not yield explosive vaporization. In the same sense it should be examined whether pressurization of a mixing zone can be excluded.

5. If the task 4 is solved successfully, the FCI-problem reduces to a violent boiling phenomenon with only mild consequences compared to those expected in 1967. If there remain any doubts, the questions of mixing the fuel with sodium and of the fragmentation process deserve further attention. Experiments can be done out of pile with relatively small portions of molten fuel.

II. Pump Coast Down

1. For the pump coast down accident one of the most important objectives would be to demonstrate the incoherence of Na-boiling and -voiding within one subassembly. This requires two-dimensional boiling models, which have to be checked in large bundle experiments. This development will have a great influence on the fuel slumping conditions in the direction of less conservatism.

2. An experiment to determine the time scale of the motion of the upper intact pin structure towards the midplane would be of great importance, since the course of the accident is seriously influenced.

3. The determination of the dominant mode, which causes loss of pin integrity, would be essential for a successful code development. In this context, it is of interest, whether fuel movement out of the regions with high neutron importance occurs and thereby reduces the reactivity level.

4. The motion of clad material after melting could be simulated in out-of-pile experiments in steam or gas environment. (This experiment would provide no information on clad freezing and channel blocking effects).

6. Concluding Remarks

If one looks into the research request list of the preceding chapter, it looks similar to those of 1967 and 1972. Based on conservative analysis, the present results, in terms of transition ramp to disassembly as well as of released energy, are of the same order of magnitude. Thus, it seems that Farmer was right with his prognosis in 1967, when he expected only little change in the description of LMFBR whole core accidents. However, this view would be rather superficial. Though the resulting numbers are about the same, considerable progress has been made in the understanding of accident sequences. One can define now much better the degree of conservatism used in the analysis. Even more important, one has recognized physical phenomena, which would help to greatly reduce the released energy if they could be included in the analysis with confidence. Furthermore, one can define, and hopefully carry out, experiments which could prove the effectiveness of those phenomena.

As an example to illustrate the degree of conservatism in the standard analysis, it should be mentioned that the 5 $/sec initiating reactivity ramp is indeed highly pessimistic for SNR 300. If all shim rods could be withdrawn simultaneously by false operation (which is not possible), a ramp rate of less than 1 $/sec results. Other conceivable incidents (e.g. fuel collapse within the clad after short overpower periods, small gas bubble entrainment in spite of a gas separator) also lead to ramp rates below 1 $/sec. The analysis of a 1 $/sec ramp shows that transition to disassembly starts after 1.5 sec. In this way, the ini-
tial time scale of the reactivity accident is similar to that of the flow coast down accident, with equally good chances for the shut-down system to operate. In conclusion, it seems that we have reached a point where a concentrated and well conducted effort, mainly in the experimental field, could help to reduce considerably the magnitude of energy release under discussion for an LMFBR.

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Fig. 1: Reactivity changes due to a Na-void of 14 cm Height around midplane in each indicated zone respectively. Comparison between 26- and 6-group calculations.
Fig. 2: Power evolution of SEFOR transient, calculated with quasistatic and point kinetic methods.

Fig. 3: Reduction of work due to parameter variation in FCI calculations.
1. Introduction

Early studies of fast reactor accidents emphasized the analysis of energetic excursions.\(^1,2,3\) Fuel compaction has long been recognized as a potential source of positive reactivity, and therefore has been used extensively as a mechanism to initiate excursions. Somewhat arbitrary, but hopefully conservative, fuel compaction modes were postulated in most early studies. For example, the EBR-II Hazards Summary\(^1\) considered a mode in which the upper half of the core fell coherently toward the somewhat densified lower half of the core. This mode was partly motivated by the notion that some of the fuel near the core center could potentially melt, run down into the cooler portion of the core, and freeze. The upper portion of the core could then fall down through the region vacated by the melted fuel and induce a prompt critical burst. The very coherent and idealized manner in which these events were treated resulted in a reactivity insertion rate of several hundred dollars per second.

The role that such hypothetical core disruptive accidents (HCDAs) should play in the overall safety evaluation of fast reactors has not always been well defined nor universally agreed upon. They have become a traditional consideration in LMFBR safety, however.

The current philosophy in the United States\(^4\) embraces three levels of safety consideration in fast reactor design. The first level emphasizes proper design and construction in order to produce an inherently safe and reliable reactor. The second level provides protection systems to ensure that any off-normal conditions can be safely arrested or accommodated. The third level considers special features that provide an additional margin of public protection in case extremely unlikely or unforeseen circumstances should arise. HCDAs are usually considered as part of this third-level evaluation.

The extent to which core disruptive accidents should be considered in reactor design is a matter of continuing debate. There is the usual trade-off between safety and economics that exists in almost any other engineered product. Providing

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\(^*\)Work performed under the auspices of the U.S. Atomic Energy Commission.

\(^**\)On leave from Purdue University.
A design that would accommodate the consequences of an overly conservative and unrealistic HCDA could impose undesirable economic penalties. On the other hand, the need for adequate public protection is obvious.

A primary difficulty in executing the third level of safety is in determining the type or range of events that should be considered as "realistic." The mechanistic approach to accident analysis is being developed to aid in making rational judgments in this area. Instead of merely postulating seemingly conservative conditions that lead to severe excursions, the mechanistic approach attempts to analyze accident sequences from some initiating event up to the conclusion of the accident.

The purpose of this paper is to briefly review the status of the mechanistic approach with an emphasis on several trends that have recently evolved. We will also comment on the type of information we feel this approach can ultimately provide.

2. The Mechanistic Approach

A mechanistic approach analyzes the step-by-step progression of accident sequences using cause and effect relationships. The first step in this approach is the delineation of those initiating events that are to be analyzed. Any identified and credible fault condition that leads to serious consequences is designed against as part of the levels 1 and 2 safety effort. Thus, the events emphasized in HCDA analysis are usually related to a postulated failure of the level-2 plant protective system.

In recent years, three specific accidents have been concentrated on. The first is referred to as a transient overpower accident (TOP). This accident is initiated by assuming a fixed rate of reactivity insertion with failure to scram. The insertion rates considered can be related to specific phenomena or faults that could potentially add reactivity, or can be selected on the basis of serving as an umbrella for unforeseen events. The second accident is an unprotected loss-of-flow accident (LOF). This accident usually assumes that the coolant pumps lose power and coastdown, and that the plant protective system fails to scram the reactor. The third type of accident involves the propagation of local fault conditions. Both fuel-pin to fuel-pin and subassembly-to-subassembly propagation are being considered. Although a number of other accidents can be readily defined, the above three types are usually singled out with the hope that other accidents will not tend to produce strongly different consequences.

The approach taken in mechanistic analysis over the past several years can be broken into the four phases shown in Fig. 1. The analysis is broken into these separate phases because quite different analysis techniques are required to solve different aspects of the overall problem. A brief description of each of these phases follows.

2.1 Accident Initiation

This phase of a mechanistic accident analysis involves a calculation of the core neutronics and thermal behavior up to the point of loss-of-subassembly geometry. Since most LMFBR designs have used rigid subassembly ducts, then intact subassemblies are only coupled through inlet and outlet thermal-hydraulic conditions and reactor power. Subassemblies with similar power, flow and irradiation conditions are generally lumped together into groups for the purpose of analysis. All the subassemblies within a given group, or "channel," are assumed to behave identically. Phenomena such as transient thermal-hydraulics, sodium boiling, fuel-pin mechanics and failure, cladding and fuel motion, and fuel-coolant interactions are treated with one-dimensional models. Multichannel codes of this type which were developed under USAEC sponsorship are the FREADM, MELT, and SAS.
systems. The original purpose of these codes was simply to give a reasonable and conservative accounting of the reactivity effects in order to provide initial conditions for the core disassembly codes, although the phenomenological modeling has become more detailed in the latest versions.

2.2 Disassembly Analysis

The disassembly phase of the calculation is entered when a prompt-critical excursion is induced. The rapid heating and vaporization of the fuel produce high pressures that disassemble the core and thereby end the power burst. The disassembly process was first described by a model developed by Bethe and Tait. In their approach, the core was treated as a homogeneous fluid so that the material motion during disassembly could be calculated using a hydrodynamic approach. The calculations were done in spherical geometry. The reactor power was calculated using point kinetics with first-order perturbation theory to estimate the reactivity feedback associated with the material motion.

A number of variations and improvements have subsequently been made to this basic approach. Doppler feedback was included, improvements were made to the equation-of-state used to estimate the pressures, more accurate neutronics were implemented in some cases, and the capability for doing calculations in two-dimensional \((r,z)\) geometry was added. A number of disassembly computer codes.
have been developed in the United States as successive improvements were made. One of the most recent of these is the VENUS-II code developed at Argonne.

VENUS-II calculates the disassembly motion by a direct numerical solution of the two-dimensional \((r,z)\) hydrodynamics equations. Since the density changes associated with material motion are explicitly calculated, a more accurate density-dependent equation of state can be employed. A number of refinements have been developed to increase the applicability of the code to the calculation of mild excursions. This will be discussed in more detail in Section 6.

2.3 Damage Evaluation

During or following reactor disassembly, the rate at which the thermal energy released in the transient can be converted to work must be established. This is used to evaluate the potential damage that the excursion can cause to the system. Work can either come from the expansion of the core materials themselves or the interaction of these materials with the sodium coolant and its consequent vaporization and expansion. Early studies of the damage potential of core disruptive accidents assumed that the work on the surrounding reactor structures would be done by the expanding fuel materials. As larger oxide-fueled reactors began to be considered it was recognized that the transfer of heat from the high temperature fuel to the sodium could considerably increase the potential work available to do damage. Hicks and Menzies pointed out the maximum work potential that could result if heat transfer to the sodium were sufficiently rapid. Since very high heat transfer rates had been observed in reactor accidents (SL-1, SPERT) attention turned to predicting these rates by assuming fragmentation of the fuel into sodium. Typically, this was done parametrically by varying the fuel particle size and mixing time and calculating the resulting heat transfer rates as a basis for assessing damage potential. Typical results of this type of calculation have been reported by Cho. An excellent review of work-energy conversion has been given by Padilla.

Since the heat transfer rates in these parametric models depends directly upon the particle size and mixing times, efforts have been made to establish the mechanisms by which mixing and fragmentation of fuel materials could take place. It now appears that these mechanisms are sufficiently slow that they are not of concern in damage evaluation but are still of interest in assessing the course of accident behavior. In another paper at this conference, Fauske will discuss our current understanding of processes by which heat transfer can be made sufficiently rapid to be of concern in reactor damage evaluations. Because of its high thermal conductivity, the possibility of high temperature molten steel interacting with the sodium must be carefully evaluated.

Once the pressure source term has been established, the response of the system can be analyzed. This is usually done with a hydrodynamic calculation of the pressure propagation coupled with an analysis of the structural response of the important system components. The REXCO series of codes developed at ANL is widely used for this purpose.

2.4 Postaccident Heat Removal

The final phase in the analysis is an evaluation of the postaccident heat removal (PAHR). The objective is to analyze the long-term decay heat removal from the fuel following a disruptive accident. That is, one ultimately wants to show how the fuel comes to rest in various parts of the system where it can be permanently cooled. This has mainly taken the form of predicting the spectrum of post-disruptive dispositions that the fuel could have, and analyzing the subsequent coolability. The disposition of core materials and the required measures to assure cooling of the core debris are, of course, dependent on the reactor design under consideration.
In particular, whether one is dealing with cooling the disrupted core with an intact primary system or whether one must assume the primary system is no longer intact makes a significant difference in the relative importance of the key phenomena involved in postaccident cooling.

Considerable technology has been developed to analyze several of the heat transfer modes expected to be important in PAHR. Predicting the heat removal from internally heated debris beds and molten pools are two of the most important technical problems. Both of these problems will be discussed in other papers to be presented at this conference.

3. Some Recent Developments in Mechanistic Analysis

While refining our mechanistic analysis of the unprotected loss-of-flow (LOF) accident in the FFTF reactor, several trends have evolved that have caused us to modify the traditional analysis framework depicted in Fig. 1. The purpose of this section is to briefly review these trends and provide an overview of a modified approach that better accommodates them. Some of the technical details will be presented in the succeeding sections. Although the examples and results presented are mainly taken from LOF analyses of the FFTF reactor, it appears that many of the trends and conclusions that emerge are generic. The applicability of the conclusions to larger reactors and other initiating events is specifically discussed in Section 4.3.

3.1 Recent Trends in LOF Analysis

As the accident initiation models have improved, the resulting initial nuclear excursions have become progressively milder. This is partly because the multichannel codes have allowed a more accurate representation of intersubassembly noncoherence. This noncoherence arises mainly from carefully accounting for the differences in power level and coolant flow rates among the subassemblies. The net result of this is that the accident sequence tends to proceed into a gradual meltdown of the core, instead of ending in a vigorous disassembly excursion when the initial fuel slumps. This does not mean that a disassembly excursion cannot be induced at some point in the accident, but merely that it is unlikely in the early stages of core meltdown.

As the subassemblies successively progress through the stage of coolant voiding and melting, the pressures generated may be too low to cause a massive dispersal of molten fuel from the core region. The subassembly duct walls are quickly melted and growing regions of molten fuel and steel begin to form in the hottest portions of the core. This stage of the accident is likely to be accompanied by a number of mild excursions induced by continued slumping in successively lagging subassemblies and possibly the reentry of fuel that was temporarily dispersed by mild pressurizations.

As the subassemblies begin to coalesce into a growing molten region, the multichannel modeling techniques used in accident initiation calculations break down for those subassemblies. On the other hand, one cannot use the disassembly analysis idealization of treating the entire core as a fluid, since much of the core may still be completely intact. We have termed this stage of the accident as the "transition phase."

Once an accident has entered the transition phase, it appears likely to proceed into whole-core involvement. Thus, ensuring an end to the neutronics events requires the permanent dispersal of a large fraction of the core. This dispersal can result from an energetic excursion ending in a classical disassembly, or can terminate from more gradual mechanisms such as boil-out or melt-out.
3.2 A Comprehensive Mechanistic Path Structure

The main elements in a more comprehensive mechanistic path structure are depicted in Fig. 2. Some of the key differences from the more traditional approach shown in Fig. 1 are as follows.

(1) The new structure allows for an "early termination" path where accidents are terminated by early negative reactivity effects such as the removal of small amounts of fuel from a largely intact geometry. This path involves relatively little energy generation and relies on the long-term in-place cooling of the bulk of the core.

(2) Instead of having disassembly as the only exit path for disruptive accidents, a more general "core disruption" analysis phase is identified. The objective of this phase is to trace the course of an accident from the disruption of the subassembly geometry to the attainment of a permanently subcritical and coolable geometry. The key problem areas addressed are:

Fig. 2. More Comprehensive Approach to Mechanistic Accident Analysis
(a) describing the fuel removal to permanent subcriticality

(b) determining the spectrum of consequences for any potential excursions, and

(c) providing detailed initial conditions for damage evaluation and postaccident heat removal analyses.

This structure allows analysis of situations where the initiating phase results in a gradual meltdown of the core, as well as the more traditional path of going directly into an energetic disassembly excursion. It also covers the situation where moderately energetic excursions disrupt the core, but do not permanently disperse the fuel. Once an accident proceeds into the transition phase, it can ultimately be ended by either a gross mechanical disassembly or by more gradual fuel removal processes.

(3) Finally, a closer coupling among the core disruption, damage evaluation and PAHR analyses is emphasized. It is important to carefully specify temperature and disposition of the fuel at the termination of the neutronics events.

4. Recent Initiating Phase Analysis Developments

As mentioned above, improved modeling of accident initiating phase phenomena and sequences has, in certain cases, led to considerations of accident paths not involving a conventional disassembly. One important part of this is the identification of the possibility for early termination of the accident through removal of small amounts of fuel from the core, followed by in-place cooling of an only partially disrupted core. Recent analyses of unprotected transient-overpower accidents have indicated that a hydraulic fuel sweepout mechanism could be effective in at least temporarily terminating the transient in some situations. Similarly, the possibility exists that fuel dispersal by fission gas or other forces could lead to a neutronic shutdown prior to disassembly in a loss-of-flow accident.

If the initial dispersive mechanisms could be established for these cases, it would still be a difficult problem to show that dispersed fuel remained dispersed and that cooling of disrupted subassemblies could be restored prior to further fuel meltdown. For a TOP type accident, with full coolant flow retained, maintaining or restoring cooling may be reasonable if the reactor power goes down. However, in a loss-of-flow situation, restoring cooling to voided subassemblies may not be possible prior to melting of fuel pins even at decay-heat levels.

4.1 Summary of Results from FFTF LOF HCDA Analyses

Most of the recent experience at ANL in analyzing HCDA's has been connected with the analysis of a loss-of-flow without scram HCDA in FFTF. The models used in the initiating phase analyses for this case are described in a separate paper. 7

Despite variations in core configurations and some of the key parameters, the FFTF LOF analyses generally have a similar sequence with the primary differences being in timing of events. Because of the power and flow distribution differences caused by the two enrichment zones (Ring 1, 2, 3, and 4 and Rings 5 and 6 respectively) and three orifice zones (Rings 1, 2, 3, and 4, Ring 5, and Ring 6 respectively), initial boiling generally occurs fairly closely together (1-2 sec) in Ring 1 and 2 subassemblies and the hottest Ring 5 subassemblies with the rest of the core following over several more seconds. Cladding dryout (primarily film stripping rather than evaporative dryout) usually follows initiation of boiling by about 1 sec and cladding melting and initiation of motion in
another 2-3 sec. Following cladding melting, fuel melting and fuel motion usually begin in about two more seconds, at least for the first fuel to melt.

Timing of the events after boiling initiation is affected strongly by the reactor power. At the initiation of boiling, the reactor power is generally at 65-75% of full power. The initial voiding reactivity is small and has little effect on the power. However, cladding motion and relocation out of the middle of the core region results in $\Delta R_{\text{core}}$ of reactivity and a power rise (as much as a factor of two times nominal power) by the time of fuel motion initiation.

In general, fuel slumping in the hottest channel leads to an initial power burst in which the reactor power reaches up to from 5-30 times nominal operating power.

These initial bursts are generally terminated within a few hundred milliseconds by a combination of several strong negative reactivity effects, including relatively strong Doppler and axial expansion feedbacks (which by themselves tend to control the peak of the burst), fuel dispersal by fission gas, sodium vapor, steel vapor, and/or fuel vapor, and, in some cases, a large negative voiding reactivity from boiling in Ring 6 due to the high heat flux during and following the burst. The rate of release of fission gas from melting fuel and the magnitude of fission gas slip among moving fuel particles used in this study are such that under nominal steady-state power conditions the irradiated high-power fuel would tend to slump slightly, form a dispersion, and eventually disperse due to fuel vapor pressures. Different results can be obtained with different fission gas release and slip assumptions in the irradiated core cases.

While the energy generation during these initial bursts may not be as large as is usually given by a conventional disassembly calculation, it is generally of the order of at least one full-power second. This rapid energy generation tends to set off a domino effect with respect to initiation of fuel motion in channels which are near melting, and, at termination of the initial burst, about one-third of the subassemblies are entering the fuel motion phase. The basic problem in these cases then becomes one of continuing the analysis into the transition phase as described above.

Since these cases involve neutronic bursts, a separate study was made to assess whether in-place cooling could be established if an early neutronic shutdown were assumed. These cases assumed early fuel removal such that no power excursions were calculated and a neutronic shutdown occurred. The SAS results were then evaluated with respect to the possibility of maintaining or recovering full cooling in all subassemblies. In summary, despite optimistic assumptions on sodium boiling noncoherence, clad plugging, and fuel removal, in-place cooling could not be shown to be established without going through a phase including fuel boiling in most voided subassemblies and probable formation of a molten region.

4.2 Development of Molten Region

As described in the previous section, an unprotected LOF accident sequence can lead to a gradual disruption of successive subassemblies. Although a series of mild excursions can accompany this sequence, no energetic excursions have been predicted in the early stages. This section discusses the extended motion and disposition of the molten fuel that is produced as the successive subassemblies melt.

The relocation and subsequent freezing of molten cladding can form blockages that inhibit the dispersal of molten fuel from the core. Following film-dryout, cladding temperatures rise rapidly and melting starts within about 2 sec under nominal conditions. The upward sodium vapor velocities predicted...
by SAS are high enough to levitate the molten cladding upward. Current models predict the molten cladding will freeze and form blockages near the top of the active core region shortly after clad melting. Following upper blockage formation, the drop in vapor velocities permits downward cladding flow, and a blockage in the lower part of the core or the lower reflector is usually predicted within less than a second after the inner blockage forms and shortly before initiation of fuel slumping. Recent experimental information on clad relocation tends to support the modeling in SAS. For example, fairly substantial upper and lower plugs were evident in the postmortem examination of the L2 experiment in TREAT.

If cladding blockages have not had time to form, or if the blockages are incomplete, the question arises as to whether molten fuel can be ejected up through the initially open upper subassembly structure. This situation has been investigated both analytically and experimentally for the case of an initially voided subassembly. The results of this investigation will be reported elsewhere, but the conclusion is that molten fuel is likely to freeze and plug after only a short penetration into the above-core structure.

If the first fuel to melt is in subassemblies that are unirradiated, then it is expected to slump and induce a mild excursion. This excursion is terminated when fuel vapor pressures disperse fuel from the center of the core. The vapor pressures are typically quite mild (several atmospheres) and cannot move the fuel beyond the blockages that form at the upper and lower ends of the core. A typical calculated fuel distribution is shown in Fig. 3. The molten fuel is

Fig. 3. Conditions Following a Mild Dispersal

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seen to temporarily densify against the blockages at the axial ends of the core region. If the first subassemblies to melt are irradiated, the fuel motion can vary depending on the nature of the irradiation and the way the fission gas influences the fuel motion. Here again, the pressures involved are sufficiently modest that immediate dispersal from the core region is unlikely. Thus, molten material begins to collect in the hottest regions of the core, and we enter the transition phase discussed in Section 3.

4.3 Other Accidents and Core Sizes

The discussion thus far has been largely based on the flow coastdown accident in FFTF. The results indicate the most likely path is from the initiating phase into the transition phase. In this section we will speculate how the initiating phase might vary for other accident sequences and reactor designs.

Considerable effort has gone into examining the TOP accident in FFTF. In this accident, the fuel pins are predicted to fail before sodium voiding has occurred. It can be argued in this case that the initial fuel to be ejected into the sodium will particulate and be permanently swept from the core region. If the negative reactivity due to this sweepout is sufficient to overcome whatever assumed reactivity insertion that is driving the accident, then the power in the system would drop. If in-place cooling of the fuel remaining in the core region can be achieved, then the accident can be ended. Therefore, the possibility of the early termination path appears more likely than for the LOF accident.

The likelihood of early termination decreases as the ramp rate during the TOP accident increases. As the ramp rate gets larger, more molten fuel is produced. It seems likely that only a limited amount of molten fuel can be swept from the core region before freezing and plugging considerations similar to those given for the LOF accident must be considered. This is born out by recent TREAT experiments to be discussed in another paper at this conference. Thus, a point is probably reached where the TOP accident would also enter the transition phase or go into gross disassembly.

When considering larger reactors, the TOP and LOF accident sequences should be quite different. As systems get larger, the sodium void reactivity becomes more positive. Thus, the initial sodium voiding could insert enough reactivity to initiate a prompt critical excursion. There should be an increased tendency to go directly into the disassembly phase similar to the more classic approach. When incoherences in sodium voiding and molten material motions are more accurately analyzed, however, less energetic excursions and a more gradual meltdown pattern could again emerge. It should also be emphasized that a transition phase condition can develop following a moderately energetic initial excursion that fails to completely disperse the fuel. In any case, it will be difficult to preclude for larger reactors disruptive sequences that take a transition-phase path, even though they may be less probable than for smaller reactors.

Finally, if a subassembly becomes molten and can propagate the disruption to its neighboring subassemblies, a transition-phase situation would develop directly. This mode of entering the transition phase would be applicable to any size reactor.

5. Preliminary Modeling of Transition Phase

5.1 Model for Growing Molten Region

Once an accident has entered the transition phase, a model to analyze the molten core region is needed. This region will consist of a mixture of molten
fuel and steel, and can be any size from one subassembly up to the whole core. In most cases of interest, the material will be boiling due to fission or decay heating. The steel will be boiling from the mixture because it has a much lower boiling point than the fuel. However, new steel will be entering the pool due to the melting attack on the surrounding structure. A preliminary numerical model has been developed to describe this situation.

The model is one-dimensional with an axial Lagrangian mesh structure. Three components are considered: molten fuel, molten steel, and steel vapor bubbles. The turbulence of the boiling process is assumed to maintain the uniformity of the fuel-steel mixture. Bubble transport up the pool is modeled. A detailed energy balance is performed that includes (1) fission heating, (2) convective heat transfer from fuel to steel, (3) convective heat loss to the pool boundaries, (4) axial conduction in the pool, (5) energy carried by steel vapor from cell to cell, (6) energy carried by steel melt-in, and (7) radiation from the top surface of the pool when appropriate.

The fission heating is axially nonuniform and uses a fixed power shape if the pool is small compared to the core, and a power shape moving with the fuel if a full core (or nearly full core) pool is being modeled. Currently, the reactivity feedback due to pool motion is determined by first-order perturbation-theory worths for a small pool or from a curve of reactivity versus pool height for a large pool.

The convective heat losses to the sides and bottom of the pool are based on the boiling-pool heat-transfer correlations used for postaccident heat-removal calculations at ANL. The purpose of including axial conduction is to model the lower nonboiling region of the pool. The energy carried across cell boundaries by vapor bubbles is calculated from the vapor flux and the heat of vaporization. The energy transfer to the surrounding structure may melt steel, which can then enter the pool at its melting point.

The model is being used to scope the transition phase of LOF accident sequences. Initial conditions for the molten region are determined from the SAS results at the time subassembly deterioration starts. The model is being used to address the following questions.

1. How rapidly do the subassembly duct structures melt as the pool forms?
2. Does the pool remain boiled up (thus keeping the fuel dispersed) or does it collapse under some conditions? If it collapses, what is the rate of reactivity addition?
3. What is the melting attack on the surrounding structure (in all directions)? How rapidly will any above-core blockages be melted away? What is the melting attack on neighboring subassemblies, control rod assemblies, or test loops?
4. How rapidly will the region pressurize if it becomes partially or completely sealed-up?
5. How rapidly will material be ejected from the pool region through various types of leakage paths?

The boiling pool model just described breaks down under prompt-burst heating conditions. If the system experiences an energetic burst, then the fuel is heated so rapidly that fuel-steel heat transfer may become unimportant and fuel vapor could become the dominant pressure source. This situation can be treated with a disassembly-type hydrodynamic model.
5.2 Coupling with SAS

To analyze mechanistically the transition phase, the above-described molten-region model must be coupled to a continuing SAS-type analysis of that portion of the core with intact subassemblies. As more subassemblies deteriorate, they would be transferred from the SAS-type of calculation to the molten region model. The net reactivity of the system must include simultaneously the material motion feedback from both the molten and intact regions of the core. Neutronic, thermal, and possibly some mechanical coupling is required. Because of the core disruption and larger material displacements expected during this phase of the analysis, multidimensional neutronics will be required.

6. Disassembly Analysis

6.1 Recent Modifications to VENUS-II

Most of the recent refinements to the VENUS-II hydrodynamic disassembly code involved increasing its applicability to milder excursions and including additional pressure sources.

A simple model to evaluate the effect of fission-gas pressure was developed. It was found that even if only a small amount of the retained fission gas could effectively pressurize and move fuel during a disassembly excursion, the energy release could be substantially reduced. This same effect has been demonstrated by others. The main questions remaining in this area are whether the fission gases are retained in the fuel through the initiation phase leading to the disassembly event, and whether they can pressurize and effectively move fuel under prompt-burst conditions. Experiments to examine these issues are being planned at ANL.

A model to evaluate the influence of a rapid molten-fuel-coolant interaction during disassembly has also been developed. The disassembly pressures are influenced by taking the thermal expansion and vaporization of sodium into account. It has been shown that the energy release during disassembly can again be substantially reduced if significant heat transfer can occur on a time scale that is short compared to the time required for a normal fuel-vapor-pressure disassembly. This time scale is on the order of a few milliseconds for a vigorous disassembly excursion. The difficulty in using this effect in accident analyses is one of establishing that rapid heat transfer of this type actually occurs under the appropriate accident conditions.

VENUS-II also includes options to increase its applicability to milder excursions. As excursions become milder, the temperatures and pressures generated are lower. Structural considerations become important and the basic assumption that the core materials can be treated as a homogeneous, isotropic fluid begins to break down. This difficulty can be partially circumvented through the use of pressure thresholds to initiate various types of motion. For example, VENUS-II has an option that restrains the radial motion at each point in the core until the radial pressure gradient across it exceeds some threshold. This option can then be used to simulate the structural influence of the subassembly ducts.

A simple model was also developed to evaluate the effect of allowing sodium to squirt out of the intact coolant channels during the early stages of a mild excursion. This allows an earlier relief of the single-phase pressures that can develop in the early stages of a disassembly calculation in a sodium filled core. Here again, the effect can be quite significant in certain cases. The reactivity effect associated with this sodium motion is small in FFTF and was neglected in the model. In larger reactors, it would become very important.
Although the Bethe-Tait method has been modified to extend its applicability to milder excursions, a point is reached where the basic approach of treating the core as a homogeneous fluid must be abandoned. On the other end of the scale, once excursions produce significant disruption of the subassembly structure, the traditional multichannel accident initiation methods begin to break down. Bridging the gap between these two approaches is the object of the transition-phase effort discussed earlier.

6.2 Extended Fuel Motion and Structural Interaction

The structural interaction considerations mentioned in the previous section were restricted to the immediate core region. These considerations are important in the initial expansion of the core materials and consequently affect the termination of the neutronics burst. These bursts are usually terminated by rather small fuel displacements (on the order of several centimeters or less). The largest displacements tend to occur near the core center with very little motion at the core boundaries. This situation is depicted in Fig. 4, where the FTF reactor is being used as an example.

Disassembly calculations are usually stopped at this point in the core expansion. In fact, the Lagrangian hydrodynamics formulation in VENUS-II precludes following the core motion much beyond the small displacements needed to end the excursion. Some spherical Bethe-Tait type calculations have been carried out to larger displacements to determine the kinetic energy ultimately attained by the material, but the interaction of this expanding material with the surrounding structure was not modeled in detail.

If the pressures generated during the excursion are not extremely large, then the surrounding structure is not grossly disturbed and the molten core materials may not be dispersed much beyond the original core region. Because of the possibility of fuel recompaction and further criticalities, it would seem to be a misnomer to apply the term "disassembly" to this situation. Disassembly has traditionally connoted an energetic and permanent dispersal of the core. As the excursion severity increases, a point is reached where the pressures are sufficiently great to disrupt the surrounding structure and rapidly disperse the core. We have termed this type of event as "mechanical disassembly."

We have found it useful to determine the excursion severity required to be at the threshold of mechanical disassembly. Knowing this threshold is useful in evaluating the range of conditions for which various modes of secondary criticality must be considered. The threshold for mechanical disassembly, as well as the form the disassembly takes, is strongly dependent on the reactor design. We will present the case of FTF as an example.

Based on numerous preliminary scoping calculations, we have estimated that the gross expansion of the FTF core following an energetic excursion would take the general form shown in Fig. 5. This diagram is only meant to provide a general depiction of the situation. The main features are that a rapid expansion of the high-pressure vapors has driven molten core materials into the above-core subassembly structure. If some combination of freezing and mechanical deformation temporarily plugs the above-core structure, then the pressure forces will tend to drive the above-core subassembly structure upward. These forces will ultimately be transferred to the instrument-tree structure, that is, directly over the core. A rapid mechanical ejection of core materials beyond this point implies sufficient upward force to deform the instrument-tree structure out of the way. The dynamic response of the instrument-tree to different loadings was analyzed. The threshold pressure and impact loading sufficient to produce a rapid gross failure of the instrument-tree structure was determined. The excursion severity required to generate this pressure loading was then found.
To calculate the structural loadings that would ultimately be produced by a given excursion, a simple model was developed to analyze the gross expansion of the core. The basic approach is similar to that used previously by the British in a code called FROTH. The core materials are divided into a number of packets. The average initial fuel temperature within each packet is determined from the VENUS-II results at the end of the neutronics burst.

The packet with the highest initial temperature is assumed to expand first. As the expansion proceeds, the temperature of the two-phase mixture decreases due to vaporization and expansion work. When the temperature of the expanding packet has decreased to the point where it is equal to the temperature of the next hottest packet, the two packets are merged. The expansion then continues with successive packets being added as the temperature in the expanding zone continues to decrease. Initially, the expansion was treated adiabatically, although some heat transfer effects have since been added. Although this approach does not predict the details of the extended fuel motion, it does provide a means for predicting the pressure and impact loadings that can be exerted on the surrounding structure following various degrees of core expansion.

The main point of this section is that the extended fuel motion and structural interaction must be considered in the analysis of mild disruptive
excursions. A core should not be considered to be "disassembled" merely because the initial neutronics excursion has been terminated by core expansion.

7. Recriticality Considerations

Recriticality must be carefully evaluated as part of any mechanistic accident analysis. This section discusses three types of recriticality that appear to be the most significant.

7.1 Continuation of Incoherent Initiating Phenomena

Disruptive accidents generally develop incoherently throughout the core. That is, localized regions are affected first, with the disruption spreading to other subassemblies as the accident proceeds. If localized phenomena cause an excursion that only disrupts part of the core, then a continuation of these (or other) phenomena in other core regions can produce another criticality at a later time. A good example of this was presented in Section 4 where the continued slumping and dispersion of successive subassemblies produced a series of mild excursions during an LOF sequence.

Recriticality of this type is the easiest to cope with in mechanistic analysis. This is mainly because the same phenomena that are normally treated in the initiation phase are the phenomena causing the recriticality. The only additional problem is accounting for the influence of any disruptions caused by previous excursions. The main difficulty here is accurately predicting the net reactivity of the disrupted system. The uncertainty in doing this increases with the number of previous excursions, or disruptions, that have occurred.

The fact remains that this first type of recriticality is caused by the motion of materials within initially intact subassemblies. As such, a relatively high and predictable degree of intersubassembly noncoherence is expected. Because of this noncoherence, the resulting ramp rates should be quite moderate. This conclusion has been borne out by the scoping studies done to date where SAS calculations have been carried out through several mild bursts. Some improvements may be needed in treating the extended fuel motion in disrupted regions before a detailed analysis can be carried out.

7.2 Autocatalytic Disassembly

In the broadest sense, autocatalysis could embrace any source of positive reactivity feedback. A particular type of autocatalysis that can potentially increase the severity of disassembly excursions occurs when the transient induced material motion temporarily increases the reactivity instead of decreasing it. Such an effect could occur during an initial excursion or as part of a recriticality.

Earlier studies\textsuperscript{15,38} of autocatalytic effects failed to uncover any physically reasonable situations that would greatly increase the disassembly energy release. These studies focused on implosive effects at the enrichment zone boundaries. Although a small amount of net positive feedback can be produced under certain idealized conditions, the general outward expansion of the core produces enough negative feedback to quickly dominate any implosive motion.\textsuperscript{38}

These autocatalytic effects are associated with the early phases of material motion. The possibility of an autocatalytic recriticality that results from gross fuel motion has also been considered.\textsuperscript{39} This mode of recriticality occurs if excursion induced pressure gradients can drive the fuel into a critical mass at some position in the reactor. An example of this would be a situation where a disassembly excursion ejects molten material out against the surrounding structure where it temporarily collects and goes critical. Although this mode
of recriticality appears conceptually possible in large reactors, it has not been shown that it could result from any realistic accident conditions. A detailed evaluation of this requires a disassembly technique that can calculate large material displacements, accurately model boundary-structure interactions, and also include an accurate reactivity assessment that considers the deformation of the space-energy distribution of the neutron flux.

7.3 Reentry of Initially Dispersed Fuel

Fuel reentry must be carefully evaluated in any core-disruptive accident. Fuel initially dispersed upward from the core region can potentially reenter via gravity, while material leaving in any direction could be driven back in by pressure sources. Reentry from above appears to be the most likely source of recriticality and will be discussed briefly here.

In order for molten material to be ejected up through and out the tops of the subassemblies, it generally must pass through a substantial distance of rather tightly packed and relatively cold structure. In FFTF, for example, this amounts to about 4 ft of wrapped pin structure in the upper reflector/fission-gas-plenum region. As was discussed in Section 4, blockages are expected to form near the top of the active core region in some situations due to molten cladding being swept upward from the core and freezing. The fuel density distribution following a mild dispersal as calculated by SAS was shown in Fig. 3. It is seen that a slug of molten fuel tends to form under the above-core blockage. This is an inherently unstable configuration and some form of redistribution of fuel back into the core region will occur.

It was also shown in Section 4 that even in the absence of a priori stainless-steel blockages, molten fuel ejected up through the upper subassembly structure tends to freeze after only a short penetration distance. Once the leading edge of the material freezes and mechanically interacts with the structure, the upward motion of the fuel can be stopped temporarily. Since the fuel in the lower portion of the slug remains molten, some form of reentry can again occur and possibly lead to recriticality.

The reactivity insertion rates associated with various modes of reentry are discussed in detail in another paper being presented at this meeting. A key parameter in evaluating the effects of recriticality is the reactivity insertion rate at prompt critical. The main factors that influence this ramp rate are: (1) the amount of fuel reentering, (2) the reentry velocity, and (3) the geometrical configuration at prompt critical.

If the disruptive events leading to upward fuel ejection are localized, then a relatively small amount of fuel will be available for potential reentry. A limiting situation with regard to the mass of moving fuel is reached with excursions that are severe enough to melt a large fraction of the core and initiate massive upward expulsion, but not energetic enough to exceed the threshold for mechanical disassembly discussed in Section 6. In this case, a large fraction of the core fuel could have its upward motion arrested in the above-core structure and become available for reentry. Setting reasonable limits for the reactivity insertion rates for this mode of reentry centers on establishing the degree of coherence and reentry velocities attainable in realistic situations. An upper limit on coherence would be obtained by assuming the fuel falls as a full-density cylindrical slug. This would be an ultraconservative approach reminiscent of the EBR-II hazards analysis.

Some of the phenomena that tend to limit the spatial and temporal reentry coherence following an excursion are as follows:

1. Excursion-induced heating and pressurization are spatially nonuniform. Therefore, molten fuel will enter the upper subassembly structure at
different times, velocities, and temperatures. This, in turn, stagger the reentry pattern between different subassemblies.

(2) There will be different degrees of above-core blockage due to relocated cladding and different amounts of molten cladding entrainment in the molten fuel from different subassemblies.

(3) Some molten fuel will freeze in the above-core structure, and thus its reentry will be delayed (or excluded completely if permanent in-place cooling occurs).

(4) Vapor and gas pressure from below inhibits reentry of dense slugs of molten material. The liquid will reflux down through the vapor, possibly in some form of rain.

Although some combination of the above kinds of effects should greatly reduce the reentry coherence, it is difficult to provide a detailed evaluation either analytically or experimentally. Techniques that can follow the extended motion of the fuel following an excursion and carefully model the structural interaction effects are needed to further investigate this problem area. Ultimately, a statistical description of the reentry could be used to provide a probability distribution for reentry ramp rates.

The second general consideration in analyzing the reentry ramp rates is determining the reentry velocities. Firma limits can be set when gravity is the only driving force that must be considered. The possibility of overpressurization that could potentially accelerate fuel back into the core region must also be carefully examined. An obvious potential source of pressurization is the interaction of the molten core materials with sodium. In the case of an LOF accident, the sodium can be voided from the core region throughout much of the core. If molten material being ejected into the above-core structure encounters liquid sodium, or even a sodium film, the subsequent thermal interaction could produce significant sodium vapor pressures. This will be further discussed in another paper at this conference. Our current understanding is that molten fuel should not interact vigorously with the sodium under these conditions. The situation with respect to molten steel at high temperatures is less clear.

8. Fuel Dispersal to Permanent Shutdown

Once an accident progresses into a gross disruption of the core, a sizable fraction of the fuel must be permanently dispersed from the core region to ensure long-term subcriticality. Like most aspects of mechanistic analysis, the specific form this dispersal takes is strongly dependent on the reactor design and accident sequence. In particular, the ease with which molten fuel can be removed from the system is strongly dependent on the impedance offered by the surrounding structure. Nevertheless, there are only a few generic modes of fuel removal. This section briefly discusses these modes and indicates some of the technical considerations that are important to their analysis.

8.1 Melt-out Through Lower Subassembly Structure

Any molten fuel that remains in the core region will try to melt and/or run down through the lower portion of the subassemblies. This form of melting attack has been actively investigated as part of the postaccident heat removal program at ANL. As molten materials try to penetrate into the relatively cold structure below the active core region, they will tend to freeze and block the passage ways. A planar melting front is expected to progress slowly downward. For example, it has been estimated that it would require from tens of minutes to hours for a pool of fuel to melt down through the FPTF lower subassembly structure at decay-heat levels. The radial melting attack on the surrounding structure
must also be considered. In general, gross dispersal via a downward melting attack appears to be a rather slow process in a reactor with an FFTF-type design.

8.2 Boil-out Through Leakage Paths

In this section we discuss the removal of fuel following a disruption of the core, but excluding those cases that involve a gross mechanical disassembly. Basically, we are focusing on possible fuel removal during the transition phase of the accident. A number of scoping calculations have been done using the boiling pool model described in Section 5 with various degrees of above-core blockage. For most cases of interest the materials in the growing molten core region are found to be vigorously boiling and pressurizing to some degree. Thus a vigorous two-phase expulsion of material would be expected through any available leakage path.

Using homogeneous two-phase flow correlations, we have calculated material ejection rates on the order of $10^6$ gm/sec for a total leakage flow area equivalent to the cross-sectional area of a single subassembly. The ejection rates vary depending on whether the flow area is a single large opening or a number of smaller leakage paths. In either case, these scoping calculations indicate that a large fraction of the molten material could be rapidly ejected if even rather small leakage paths are available.

The main difficulty in analyzing this mode of fuel removal is in identifying the leakage paths and ensuring that they would remain open. As discussed earlier, molten material ejected through small openings in the relatively cold above-core structure will tend to freeze and plug. These plugs should only be temporary, however, since they would melt out due to a combination of decay heat and continued heat transfer from the boiling materials remaining in the core.

Preliminary calculations done with our transition-phase boiling pool model have indicated that above-core blockages would be rapidly melted away (on the order of seconds). In general, these calculations indicate that for the FFTF subassembly design, the entire upper reflector and fission-gas-plenum region would be melted away on a time scale that is short compared to the time required to melt down through the bottom of the subassemblies. Thus, it is our current judgment that upward leakage paths are bound to develop and that this mode of fuel removal would be dominant in the situation where high pressurization and gross mechanical effects are not involved.

8.3 Mechanical Disassembly

If an energetic enough excursion is induced during an accident sequence, then a large fraction of the fuel can be rapidly removed via a mechanical disassembly. Here again, the manner in which this occurs depends on the reactor design. As mentioned earlier, the gross disassembly motion in FFTF is expected to be predominantly upward. A clear path from the core region into the above-core sodium would be created by such a disassembly event. Part of the fuel would be ejected in the initial event with a continued ejection, or blowdown, of much of the remaining fuel as the core region depressurizes.

A mechanical disruption of the above-core structure could also happen if the structure surrounding the core region can become sufficiently plugged following a disruption of the core. The core region can then pressurize due to vapor production. The rate at which this occurs is highly dependent on the power level, and therefore the reactivity history, during the accident. Such a mechanical disruption would not be greatly different than an excursion induced mechanical disassembly, except that it could possibly be more localized.
9. Model Development Trends

This section briefly summarizes trends in the model development work being performed at Argonne National Laboratory.

9.1 Initiation Phase

In the initiating accident analysis area, model development is well underway, although much work remains in relating models to experimental information. Another remaining major area of work is developing and verifying models which treat intrasubassembly noncoherence. As the understanding and modeling improves, it will become reasonable to treat both physically distributed stochastic events and the uncertainties involved in model parameters in order to provide the basis for probabilistic analyses of accident sequences. In addition to the phenomenological modeling, improvements to the neutronics calculations will be emphasized. In particular, a space-dependent kinetics capability will be added to SAS. Initially this will take the form of linking SAS to the two-dimensional quasi-static code, FX2.42

9.2 Transition Phase

The main effort in transition-phase modeling is coupling a molten region model with a SAS treatment of intact subassemblies. Neutronic, thermal, and some mechanical coupling will be included. The coupled model will allow subassemblies to coalesce with neighboring pools as their duct walls deteriorate. The influence of control rod or test-loop channels is being considered. Space-dependent neutronics must also be incorporated.

The boiling pool model currently used to describe the growing molten region will be refined. Special attention will be given to describing the pool response to mild excursions.

The objective of this work will be to provide a more mechanistic analysis of the transition phase. A careful evaluation of the reactivity and power history is needed to determine the ultimate path to shutdown.

9.3 Disassembly Analysis

The main need in the disassembly area is to provide models that include the following features:

(1) The ability to calculate extended (large displacement) material motion.

(2) More accurate space-dependent neutronics (at least 2-D).

(3) Improved modeling of interaction of expanding core materials with the surrounding structure.

Models with the above features can be used to provide increased understanding of autocatalytic effects as well as the whole recriticality problem.

9.4 Damage Evaluation

Improved modeling of the interaction of molten core materials (fuel and steel) with sodium is under way. An improved phenomenological understanding is being sought, as well as developing models that are closely coupled with the core disruption analysis so that specific geometrical conditions can be addressed. The objective of this work is to provide a more accurate pressure source for damage evaluation calculations. While more work remains to be done, it would appear...
that at least for the voided core case we are returning to the position of assess-
ing damage following a core disruptive accident based primarily on fuel vapor
expansion. Heat transfer from the expanding materials is still important, how-
ever, and will continue to receive attention.

Future work in the structural response area will concentrate on extend-
ing the analysis beyond the primary vessel. Sodium leakage, piping response, and
secondary containment evaluation are main areas of concern. A more detailed
description of this effort is given in another paper at this conference. 43

9.5 Postaccident Heat Removal

Improved heat transfer correlations for internally heated debris 23 beds
and molten pools 22 (boiling and nonboiling) will be developed. The interaction
of molten fuel with sacrificial materials will be investigated. The long-term
disposition of fission products will be examined to improve decay-heat assessments.

10. Viewpoint on Deterministic vs Probabilistic Treatment of Accident Progression

The mechanistic analysis of nuclear accidents, including ECDAs, provides part
of the information that is ultimately needed to make a rational judgment about the
safety of a given reactor. The goal is to determine the consequences that result
from specific initiating fault conditions. This information can then be coupled
with the probability that the given fault conditions might arise, to arrive at a
risk assessment of those events.

In recent years, the mechanistic analysis has often been viewed, or at least
idealized, as being deterministic in the sense that a detailed single path could
be traced from the initiating event to the final consequence. It was recognized
that uncertainties would always remain, but it was hoped that their influence
could be evaluated by repeating the single-path deterministic analyses with vari-
atations of the key parameters.

We currently feel that this single-path approach is not feasible for the large
class of hypothetical accidents which involve complicated core disruptions during
the progression of the accident. Since accidents of this type may play an import-
ant role in the overall safety assessment, a general accident analysis approach
must be suitable for the treatment of these more complicated cases.

The straight-through approach might be viable for accidents that take the
early-termination path, since the core geometry remains almost entirely intact and
most of the phenomena can be quite well defined analytically and experimentally.
Also, accidents at the other end of the spectrum may be amenable to this approach.
These would be accidents that terminate by very vigorous disassembly excursions
that are induced early in the accident sequence. In such a case, the conditions
leading to the disassembly can be quite accurately analyzed since they are again
occurring in a well defined geometry. Also, the vigorous disassembly itself can
be fairly cleanly analyzed because structural details become unimportant.

Many accident sequences of interest will fall between these two extremes. In
such cases, a more gradual disruption of the core ensues. The analysis becomes so
complex that a single-path evaluation of the entire scope of events does not seem
to be feasible in any practical sense. It is then necessary to augment the deter-
mindistic approach by introducing probability distribution functions to describe
the spectrum of possibilities of complex phenomena. Including such probabilities
widens the range of consequences from a given initiating fault condition.

Other phenomena which contribute to the spread of the predicted consequences
of an accident caused by a single initiation are the uncertainties in all param-
eters and computational models. Due to the highly nonlinear parameter dependencies
of severe fast reactor accidents, the result of the superposition of the various uncertainties is difficult to predict without an appropriate explicit consideration. In accidents, or accident phases, in which the single-path deterministic approach is applicable, the investigation of the impact of parameter uncertainties is essentially a large sensitivity analysis. In all cases, however, in which the deterministic path of the progression is broken due to the complexity of a given phase, a more general method is called for. Such a method in which deterministic path treatments are combined with probabilistically described features may be called a "semi-deterministic" treatment of accident progressions.

The investigation of accident sequences including their uncertainties provides the proper basis for an overall probabilistic safety assessment. Initially, many of the important distribution functions may be very poorly known. Plausible idealized distributions can then be assumed to bracket the uncertainty of the detailed accident progression. Investigations with assumed distributions may also help to identify particularly sensitive superpositions which warrant special research attention and emphasis. In order to reduce these uncertainties a continued effort is required to understand and describe accident sequences in as much detail as possible. As further experimental knowledge and more refined models become available, they will be applied to narrow the distribution functions used to describe certain phenomena and uncertainties.

11. Summary

As the mechanistic analyses of the initiating phase of core disruptive accidents have become more sophisticated, the initial excursions predicted have tended to be less energetic. This is expected since less sophisticated methods often have to apply undue conservatism. If the excursions can become so mild that the accident is not permanently terminated by an early thorough dispersal of the core materials, then the final shutdown is achieved in a subsequent, and often complicated, progression of the accident.

Models are currently under development to address this disruptive phase of accidents. The key problem areas are describing the fuel removal to permanent subcriticality and assessing the consequences of the accident sequence to the system. A more comprehensive structure for pursuing the analysis has been presented in this paper. Considerable progress has been made in several areas of modeling with current trends emphasizing improved descriptions of transverse fuel motion and more accurate neutronics.

The general complexity encountered in analyzing gross disruption sequences suggests that a single-path deterministic analysis is not the most effective approach. A "semi-deterministic" approach that uses distribution functions to describe complex phenomena is briefly discussed. A full-scale development of this approach is a long-term project. However, implementation of the basic conceptual framework has short-term advantages compared to a single-path approach. Further safety evaluations of fast reactors currently under design may require the treatment of accident phases for which, at least currently, no detailed straight-through deterministic analysis is available. The "semi-deterministic" approach will provide the basic conceptual framework for an appropriate scoping investigation to find the range of consequences.

References


40. J. E. Boudreau and J. F. Jackson, "Recriticality Considerations in LMFBR Accidents," These Proceedings.


RECRITICALITY CONSIDERATIONS IN LMFBR ACCIDENTS

by

Jay E. Boudreau, Los Alamos Scientific Laboratory
J. F. Jackson, Argonne National Laboratory

ABSTRACT

Recent studies suggest that fuel which is dispersed upward by a mild prompt critical burst may reenter the core region causing a secondary excursion. In this paper, several modes of fuel reentry recriticality are examined, and phenomena which strongly affect reactivity ramp rate estimates are evaluated. A broad range of ramp rates results from these studies. The results also indicate that a number of effects expected to be present in realistic situations can strongly mitigate the reactivity insertion rates.

I. INTRODUCTION

The possibility of secondary criticality during hypothetical core disruptive accidents (HCDA's) has become a major concern in LMFBR safety analysis. Intensive research efforts over the past few years have begun to explore the physics of recriticality in an attempt to place this phenomenon in context with other safety issues pertinent to the LMFBR. The reassembly of molten fuel during an accident is a difficult process to predict accurately. Substantial uncertainties are present at every step of the analysis. Consequently, in reporting early results in recriticality analysis, one runs the risk of being misinterpreted. These scoping analyses are intended to provide a range of reactivity ramp rate estimates. The maximum ramp rates reported here result from idealizations and are not intended to typify realistic recriticality estimates. These analyses do not produce a definitive mechanistic analysis. Neither do they indicate the likelihood of the initiating accident. The probability of a loss of flow or overpower transient without scram is the subject of other studies. These probabilities must be coupled with the consequences of the HCDA's in order to assess the risk of the accident to the public.

We present in this paper a synopsis of the scoping studies on recriticality which have been performed to date along with some of the early results. We also isolate and evaluate the important phenomena in assessing recriticality ramp rates. This should provide guidance in future efforts to analyze the problem. Our work has been motivated by recent accident initiation studies for an unprotected flow coastdown in a small LMFBR. Our current opinion is that reentry recriticality may well be a problem for larger reactors and for a variety of accident initiators. The first section of the paper deals with the prediassembly stage of the accident. Both the unprotected loss of flow and overpower transient are discussed in terms of their intrinsic tendency to produce weak material dispersion and to cause significant quantities of fuel to become mobile. The next section explores some of the more important aspects of the material dynamics resulting from the mild prompt critical burst that follows the accident initiation stage. Secondary criticality is explored next, including recriticality in the core region and below the core. The modes of recriticality assumed are intended to provide a broad range of results that may be used to gain some perspective on the range of reactivity ramp rates that may occur in recriticality and to evalu-
ate the effect of several phenomena expected to be important in fuel reentry dynamics. This section also provides insight into the amount of fuel required for criticality in various geometries and locations. In the next section VENUS-II\textsuperscript{1} disassembly code results based on recriticality conditions taken from a reentry analysis are reported. Reactivity ramp rates that reflect both mild and strong bursts are chosen to provide some feel for the range of thermophysical conditions that might result from an energetic secondary excursion. The potential mechanisms for permanent dispersion of core materials are briefly explored in the following section. The last two sections emphasize uncertainties in the results presented, suggest key research areas and list conclusions.

We emphasize that the various accident sequences discussed throughout the paper are based on models, that the results cited are derived from computer calculations based on these models, and that we are not now drawing any definitive conclusions about the behavior of a real reactor.

II. EARLY STAGES OF THE HCDA

In this section, we briefly discuss the features of two unprotected LMFBR accidents that may lead to fuel reentry into the core and to secondary excursions.

A. Loss of Flow without Scram

The sequence of events in the loss of flow HCDA is sensitive to the given LMFBR design as well as to a number of uncertain reactor parameters. These parameters include fuel, steel and sodium reactivity worths and reactor loading patterns as a function of burnup, as well as coolant channel orificing variations. Furthermore, there are major uncertainties in some of the physical processes that are likely to be relevant during predisassembly. Nonetheless, in small reactors, certain phenomena are found to recur in a wide variety of flow coastdown calculations using the SAS-2B\textsuperscript{2} code. Typically, the chronology of a flow coastdown in a small LMFBR includes extensive sodium boiling and voiding in the core region followed by cladding steel melting and relocation and finally fuel melting and relocation. Fuel relocation, in conjunction with other reactivity effects, may cause the reactor to become prompt critical at the rate of a few tens of $/\text{sec}$. In larger reactors sodium void and steel relocation worths are larger than in small reactors. This sometimes results in a prompt critical excursion early in the coastdown and before appreciable fuel melting and slumping has occurred.

Both large and small reactors often encounter a prompt critical excursion at some point in the coastdown. This excursion can melt large portions of the core quite coherently and will tend to disperse some fraction of the fuel, depending on the strength of the burst. If the burst is large enough, the core will disassemble in the classical sense. That is, the burst will rapidly remove large amounts of fuel from the core region, pushing away the structure that surrounds and contains the core. Milder bursts, however, only serve to make large amounts of fuel mobile and therefore available for recriticality. Furthermore, escape routes tend to become plugged by the freezing of molten material in the cold surrounding structure. This characteristic of mild bursts inhibits permanent dispersion of the fuel by bottling up the reactor.\textsuperscript{4,5}

B. Overpower Transient without Scram

The physics and timing of the overpower transient HCDA are substantially different from those in the loss of flow case. The sequence of events usually involves rapid fuel heating, localized fuel pin failure, expulsion of molten fuel into the coolant channel, and a molten fuel coolant interaction (MFCI) which in mild ramp rate cases may void the channel rapidly, dragging fuel fragments with the bulk sodium. Once again, the sequence of events depends strongly on uncertain reactor parameters and processes. In addition, statistical uncer-
tainties enter the overpower transient analysis since the fuel pin failure location may depend on manufacturing defects and operating history. In larger reactors, as in the loss of flow case, the sequence may be entirely different from that in small reactors. In small reactors, prompt criticality may not occur at all during the overpower transient, whereas in large reactors it is more likely.

The overpower transient HCDA may lead to one of two situations. The first possibility is permanent neutronic shutdown. This may result either from a classical disassembly or from a mild burst which sweeps some of the fragmented molten fuel out of the core and plenum, leaving coolant channel geometry essentially intact and permitting in-place cooling. The second situation that may occur sets the stage for recriticality. In this case, coolant channels become clogged and partially blocked by fuel fragments that have become lodged on cladding and wire wraps on their way out. Alternately, coolant channel geometry may be disrupted by mechanical deformation during the MFCI stage of the accident. Core cooling is impaired if either situation occurs, and a power flow mismatch may lead to sodium boiling and voiding followed by steel and fuel melting as in the loss of flow case. The degradation of cooling is likely to occur only in selected subassemblies and fuel melting and relocation may lead to a negative reactivity insertion or to a mild prompt critical burst. If a mild burst occurs a large amount of fuel can become molten quite coherently, and the likelihood of further criticality events increases unless permanent dispersion is achieved.

We see, then, that in either the loss of flow or the overpower transient HCDA's, recriticality may result from the motion of the core material that has become mobile due to melting. Assuming that the initial prompt critical burst that occurs is insufficient to mechanically disassemble the reactor (see Sec. VI.A), we are faced with the task of analyzing weak dispersion material dynamics. Some considerations of the physics of this process are presented in the next section.

III. WEAK DISPERSION MATERIAL DYNAMICS

A. Fuel Freezing and Plugging in the Fission Gas Plenum

Figure 1 depicts the internals of a typical, small LMPBR. This model, along with the information presented in Appendix I, will be used throughout the remainder of the paper in various calculations. The model is based on an FTR type reactor.

The loss of flow without scram HCDA will be assumed in the results which follow. The specific sequence of events one expects in the predisassembly stage of these accidents is discussed elsewhere in the literature.

The lower axial and radial shields shown in Fig. 1 are massive steel structures and tend to inhibit strongly downward and radial fuel motion following a mild prompt critical burst. Initially intact hex cans also tend to telescope material dispersion preferentially in the axial direction. Coolant channels in the lower shield region tend to plug rapidly when molten material attempts to pass through the channels. This plugging results from the heat capacity of the shield along with the cooling that occurs as sodium attempts to reenter the core during the coastdown.

Hence when the material in the core generates pressure during an excursion, net preferential upward motion tends to occur. The fate of the ejected material is of key importance in the study of recriticality.
The fission gas plenum region shown in Fig. 1 provides a large heat transfer area to the molten fuel being ejected. Freezing of the fuel may occur, potentially plugging the upper ends of the subassemblies, thereby closing off an escape route for future bursts. An initial model to analyze the freezing of ejected molten fuel in the plenum following a flow coastdown has been developed. The model predicts that the leading edge of an upward moving fuel slug will tend to freeze by the time it has penetrated about one foot into the plenum. This is found to be true for initial fuel temperatures up to 380°C and constant driving pressures up to 100 atm. Turbulent heat transfer correlations are used to obtain this result. Two mechanisms could potentially limit the heat transfer rate from the fuel slug to the plenum steel. First, a thin film of fuel may freeze on the steel surface and insulate the fuel slug from further heat loss. The second mechanism involves the sodium film which remains on the plenum steel after bulk sodium voiding.

These two mechanisms are currently under investigation and will be explored in a future paper. Preliminary studies indicate the two mechanisms may reduce heat transfer rates somewhat, but that the general conclusion that fuel will freeze before leaving the plenum still applies.

Hence, initial calculations and experiments indicate that fuel which is being ejected upward as a result of a mild excursion (20 to 50 $/\text{sec or so}) will freeze and lodge in the plenum region. The mechanical effects involved in fuel lodging are not thoroughly understood.

B. The Effect of Sodium Vapor Generation on Fuel Dynamics

In the analysis of fuel freezing in the upper plenum it is assumed that there are no extraneous forces acting on the fuel slug from above. If sodium vapor is rapidly generated above the slug (see Fig. 2) due to heat transfer between a sodium film and the leading edge of the molten fuel (or steel) the concomitant generated pressure may act to alter the dynamics of the slug and to preclude freezing and lodging.
Clearly, the effect of fuel slug dynamics depends on the dynamic pressure difference across the slug. Pressure that is built up above the slug allows down the fuel and pushes the bulk sodium level up. The position of the bulk sodium level shown in Fig. 2 is an important parameter in this process. If the bulk sodium is at the lower end of the plenum when the fuel slug enters, then heating and pressurization is possible. Hot vapors in the core region below the slug serve as the upward driving pressure. These vapors will tend to condense and expand as the slug accelerates away from the hot core and toward the cold plenum structure. Hence the driving pressure will drop as the slug moves upward. The combined pressure variations from above and below the slug may act to turn the slug around and drive it back down into the core before it freezes and becomes lodged in the structure. Some aspects of this process are discussed in Appendix II. Fuel slug dynamics in the upper plenum may depend strongly on the rate at which sodium vapor is produced, but the details of the entire process are not well understood at this time.

C. Condensation and Vaporization Rates

As indicated above, weak dispersion material dynamics is quite sensitive to relatively mild variations in driving pressures. The rate of vapor production from various heat transfer processes in conjunction with the rate of condensation of these vapors may play a role in determining the dynamic pressures acting on ejected material and on the above core structure. Equilibrium thermodynamics may be inappropriate and the accurate prediction of the kinetic processes that are involved is difficult. The relevance of these processes to the problem at hand should be evaluated in future studies.

D. Other Pressure Sources

Two other sources of pressure are likely to play a role during weak dispersion dynamics. They are fission gas and stainless steel vapor.

The amount of volatile fission gas present at any instant depends on the amount of burnup in the reactor. For the reactor shown in Fig. 1, fission product gases migrate up the fuel-cladding gap into the large fission gas plenum region. The pressure in this plenum region may be as large as 80 atm at standard operating conditions before refueling occurs. The less volatile fission products can vaporize during a transient and provide substantial pressure at temperatures above 5000°K. In a loss of flow HCDA, some of the volatile gas is likely to be released during the predisassembly portion of the accident. It may therefore be present in the weak dispersion stage of the accident as a noncondensible gas which retards the condensation of other vapors. The precise role of fission gas during the accident is not completely understood at this time.

Steel vapor is likely to be produced during the accident since heat transfer rates are quite rapid and since steel boils at atmospheric pressure at about the melting temperature of fuel. The amount of steel vapor which is formed, however, is uncertain and should be evaluated in future studies.

Sodium vapor may be generated in the core region during fuel motion since liquid sodium is likely to be present in unvoided test loops, control assemblies and between subassemblies. If the subassembly walls fail at some point during fuel dispersion, the sodium vapor produced could help to eject core material more energetically.

The uncertainties in weak dispersion dynamics preclude precise definition of extended core material motion at this time. Consequently, a variety of assumptions must be made in order to make scoping calculations of recriticality. The most likely regions for recriticality appear to be in and below the reactor core. In the next section, several modes of recriticality will be postulated in an attempt to explore potential secondary criticality in a small LMFBR.
IV. Postulated Modes of Secondary Criticality and Reactivity Ramp Rate Estimates

Several modes of recriticality are examined in this section. Our purpose here is not to discuss whether or not a given type of recriticality event will occur during a particular hypothetical accident situation, but rather to examine the reactivity effects associated with a few generic modes that we currently judge to be the most probable. For convenience, we have broken the discussion into two parts. Initially, we will examine recriticality events that occur within, or near, the original active fueled region of the core. Events in this region would occur during, or shortly after, fuel melting and core geometry disruption. Later, we will briefly discuss the possibility of recriticality occurring in other regions of the reactor vessel following the gross relocation of core materials. This relocation can result from pressure driven dispersal and/or downward melting of molten fuel following a disruptive accident.

It should be emphasized that the propensity for a particular mode of recriticality to develop, as well as the specific course it might take, is strongly dependent on both the specific reactor design and accident sequence being considered.

A. Recriticality in the Core Region

There are a number of modes of recriticality that could potentially develop in the core region during a hypothetical disruptive accident. Two of these modes will not be discussed in this paper but are mentioned below. First, continued core slumping could lead to recriticality. For example, in the unprotected loss of flow accident small amounts of fuel may be ejected from the core, temporarily shutting the reactor down. A power/flow mismatch usually occurs in the remainder of the core, however, and further core slumping may occur leading to a secondary excursion. A second mode of recriticality may result from autocatalytic effects during disassembly. This effect is more likely in larger reactors and is discussed elsewhere in the literature. A third mode of recriticality appears to be the most troublesome and will be discussed in this paper. In this mode, we are concerned with the reentry of molten fuel initially dispersed into the above core subassembly structure by pressures that develop in the core region. A detailed discussion of how this situation might evolve is given elsewhere. The essential features are as follows:

1) Molten fuel is dispersed upward into the above-core subassembly structure by pressures that develop in the core region.

2) The molten fuel is unable to pass up through and out of the tops of the subassemblies because of blockages and/or overpressurization that can develop. Blockages can arise from the freezing of molten materials and/or mechanical jamming. Overpressurization can potentially develop if the molten materials encounter sodium in the upper subassembly region.

3) The lower portion of the core region is expected to become blocked by the freezing of molten materials as they encounter the cold structure below the core. Since this lower structure is typically rather massive, an effective barrier to downward fuel motion is expected to develop. Fuel that collects in the lower core region could eventually melt down through the lower structure due to fission and/or decay heating, but the time scale for this is rather long.

4) Unless the fuel initially dispersed upward and lodged in the above-core subassembly structure is permanently frozen in place, it will begin to reenter the core region and join with the fuel being held up in the lower core region by the below-core blockages.

5) As the fuel reenters the core region a prompt-critical excursion can be induced. The purpose here is to evaluate the reactivity effects associated with various types of reentry. The reactor model presented in Appendix I will be used for computational purposes.
As a first example, consider a reentry situation that evolved as part of a recent study of an unprotected flow-coastdown accident. The initiation phase of the accident was calculated using the SAS-2A code. The accident sequence proceeded through the voiding of the sodium and subsequent melting of cladding material and fuel in the central three rings of subassemblies. As the fuel slumped, a mild prompt-critical burst was induced that rapidly heated the fuel. This burst was terminated by the negative doppler effect and the dispersal of the fuel in the central three rings. At the time of the initial fuel dispersal, it was predicted that the remainder of the core was also voided of sodium except for the outermost ring. Although some steel and fuel melting was calculated to occur in rings 4 and 5 no gross fuel motion had yet occurred.

The idealized reentry geometry that results from this case is depicted in Fig. 3.

A number of the details inherent in this idealization are subject to question. For example, the exact shape, density distribution and steel content of the reentering material depend strongly on the detailed thermal and mechanical effects that occur when the molten fuel penetrates the upper structure. The mode of reentry is also influenced by the presence of vapor and possibly noncondensible gases in the core region. The exact disposition of the lower fuel pool is also subject to question. Later we will isolate some of these major uncertainties and evaluate their influence on the reactivity effects associated with reentry.

The major assumptions in our computational model are:

1) Fuel motion is strictly within the driver subassemblies - all loops and control rod ducts remain intact.

2) Stainless steel from the lower half of rings 1-3 has run down into the relatively cold lower axial reflector, freezing there.
3) A 24-cm high fuel puddle has formed on top of the plugged lower reflector.

4) The upper half of the ring 1-3 fuel has been ejected upward, melting adjacent steel as it proceeds. All of the steel within the subassembly wall between the core midplane and the top of the fission gas plenum has homogeneously mixed with the ejected fuel slug. The resultant slug is 62 cm high.

5) All of the active core clad steel in rings 4 and 5 has melted and run down, filling the lower axial reflector and forming a steel puddle 16.7 cm deep in the core region.

6) Ring 6 is fully intact and not voided of sodium.

7) The control rods are assumed to be in their fully withdrawn position.

The net reactivity of the system as a function of the distance the fuel/steel slug has penetrated into the core region is shown in Fig. 4. The calculations were done using the DIF2P13 two-dimensional diffusion code and the cross section set described in Appendix I. The reactivity values are referred to the \( k_{\text{eff}} \) calculated for the sodium voided unperturbed geometry.

The slope of the curve at the point of prompt criticality is \( \Delta \rho/\Delta X = 0.14 \) \$/cm. The translation of this result into a reactivity ramp rate hinges on the specification of the slug velocity at the position of prompt criticality. The range of velocities that might be expected for various situations will be discussed later.

Examination of Fig. 4 shows that the slope of the reactivity vs. position curve varies significantly over the range of the calculations. Calculations on other reentry modes have shown even more nonlinearity. The reactivity insertion rate at prompt critical is one of the most important parameters in determining the severity of a prompt-burst excursion. Since this ramp rate depends on both \( \Delta \rho/\Delta X \) and the reentry velocity, it is apparent that the consequences of a given reentry mode depend strongly on the position at which prompt criticality is reached.
This is illustrated in the following example in which the first 5 rings of fuel are all involved in completely coherent reassembly. A relatively conservative assessment of recriticality is obtained for this example. TWOTRAN-II was used with an S-4 quadrature set and 4 group cross sections in this study. A cylindrical slug of compacted fuel weighing 618 Kg is assumed to fall toward the remaining ring 1-5 fuel which has puddled on top of the lower axial shield. The reactor is prompt critical with the slug and puddle separated by a distance of 76.6 cm. If fuel is removed from the puddle, the slug-puddle separation distance for prompt criticality decreases. Table I shows the reactivity gain in $/cm for lowering the slug as a function of separation distance at prompt critical.

TABLE I

The Effect of Slug-Puddle Coupling on Reactivity Gain

<table>
<thead>
<tr>
<th>Separation distance (cm)</th>
<th>Reactivity gain ($/cm)</th>
<th>Percentage of total rings 1-5 fuel inventory removed</th>
</tr>
</thead>
<tbody>
<tr>
<td>76.6</td>
<td>0.07</td>
<td>0%</td>
</tr>
<tr>
<td>51.0</td>
<td>0.33</td>
<td>13%</td>
</tr>
<tr>
<td>24.5</td>
<td>0.67</td>
<td>20%</td>
</tr>
<tr>
<td>5.0</td>
<td>1.90</td>
<td>32%</td>
</tr>
</tbody>
</table>

Hence we see that the reactivity gain increases greatly as the system becomes more tightly coupled.

Next we ask what effect dilution with molten stainless steel might have on reentry criticality. One can imagine a good deal of steel melting and mixing with fuel during the weak dispersion dynamics phase. All of the steel in the core, as well as the steel in the upper axial reflector and around 1/4 of the fission gas plenum may be in contact with molten fuel at some time. To exaggerate the effect of steel dilution, we mix all of the available fuel in the slug and puddle of the above 5 ring case in the following arbitrary manner. The clad and subassembly steel in the ring 1-5 fuel region is mixed uniformly with steel in the lower reflector and the fuel in the puddle. The steel in the upper reflector and the lower 25% of the plenum is similarly mixed with the fuel in the slug. It is noted that the assumption of homogeneous molten fuel-steel mixtures for the slug and puddle was a simplification, justified solely because of the exploratory nature of these calculations. Actually, the melting of steel by molten fuel debris is a complicated phenomena involving detailed heat transfer considerations.

The calculations for the above molten fuel-steel geometries were performed as follows. In all cases, the fuel mass and steel mass in the slug was held constant. The mass of the fuel in the puddle was varied and, with one exception, the mass of steel was kept fixed. The calculational results are presented in Table II along with data for the 5.0 cm separation distance case of Table I, for reference. First, consider cases B, C, and E of Table II. As the amount of fuel removed from the puddle increases from 0.0 to 7%, the separation distance at prompt critical is decreased and the reactivity change ($/cm) increases. However, with 14% of the fuel removed, the system was subcritical even at zero separation. We note that for case A, in which the fuel dilution was much less than case E, removal of as much as 32% of the fuel in the puddle still gave a separation distance of 5 cm and $1.9/cm for the reactivity change. Case D in Table II was the same as case C except that the steel in the latter problem was replaced by void. This resulted in a small increase in the separation distance and a 25% decrease in the reactivity worth per cm. For the problem considered, replacing stainless steel by void did not significantly alter the results. Insofar as the reactivity worth per cm is concerned, we conclude that, for approxi-
TABLE II
Calculational Results for Several Postulated Recriticality Conditions

<table>
<thead>
<tr>
<th>Slug-puddle separation (cm)</th>
<th>Case A</th>
<th>Case B</th>
<th>Case C</th>
<th>Case D</th>
<th>Case E</th>
</tr>
</thead>
<tbody>
<tr>
<td>Mass fraction of total fuel in rings 1 to 5 -slug</td>
<td>0.33</td>
<td>0.33</td>
<td>0.33</td>
<td>0.33</td>
<td>0.33</td>
</tr>
<tr>
<td>-puddle</td>
<td>0.35</td>
<td>0.67</td>
<td>0.60</td>
<td>0.60</td>
<td>0.53</td>
</tr>
<tr>
<td>-removed</td>
<td>0.32</td>
<td>0.00</td>
<td>0.07</td>
<td>0.07</td>
<td>0.14</td>
</tr>
<tr>
<td>Fuel vol. fraction -slug</td>
<td>0.88</td>
<td>0.38</td>
<td>0.38</td>
<td>0.38</td>
<td>0.38</td>
</tr>
<tr>
<td>-puddle</td>
<td>0.64</td>
<td>0.56</td>
<td>0.53</td>
<td>0.53</td>
<td>0.50</td>
</tr>
<tr>
<td>Steel vol. fraction -slug</td>
<td>0.12</td>
<td>0.62</td>
<td>0.62</td>
<td>0.00</td>
<td>0.62</td>
</tr>
<tr>
<td>-puddle</td>
<td>0.36</td>
<td>0.44</td>
<td>0.47</td>
<td>0.00</td>
<td>0.50</td>
</tr>
<tr>
<td>K-effective</td>
<td>prompt critical</td>
<td>prompt critical</td>
<td>prompt critical</td>
<td>prompt critical</td>
<td>$19 below pr. crit.</td>
</tr>
<tr>
<td>$/cm at prompt critical</td>
<td>1.9</td>
<td>0.3</td>
<td>0.8</td>
<td>0.6</td>
<td></td>
</tr>
</tbody>
</table>

nately the same separation distance, a twofold reduction is achieved by diluting the fuel in the slug and puddle with all of the stainless steel available for mixing. For more realistic steel fuel dilutions, the effect would be less pronounced.

Although the coherent reentry of a cylinder of dense fuel and steel could be considered to be a useful idealization for determining the upper limits on the reentry reactivity insertion rates, a considerable amount of noncoherence would be expected in a realistic situation. Some of the phenomena that may lead to noncoherence are the following.

1) The molten fuel is ejected from the core region by spatially varying pressure sources. Thus, the molten fuel enters the above core structure at different times and with different velocities, temperatures and molten steel content.

2) In most cases, the molten materials would be expected to penetrate and subsequently reenter from within intact, or partially intact, above core duct structure. Therefore, the fuel within each subassembly would be nearly thermally and mechanically independent.

3) Pressures which remain in the core region may cause reentering fuel to behave in an unstable fashion, and to run down assembly walls.

4) Some fuel may remain frozen and lodged in the upper structure, causing the fuel which melts to fall down in a rain-like fashion.

Some aspects of noncoherent reentry are investigated in the following recriticality calculations.

Let us isolate the effects important in assessing the reactivity gain due to the reentry of fuel. First, consider a central subassembly as shown in Fig. 5. The fuel slug of height $L$ consists of a homogeneous mixture of fuel and steel. In this case, the slug consists of a mixture of 1/2 of the fuel in a driver subassembly with all of the inconel in the upper reflector as well as all of the steel in the first 38 cm of the plenum plus 50% of the core hex clad steel and 65% of the core clad steel. This gives rise to a fuel slug with $L = 52.7$ cm. The puddle consists of 1/2 of the core fuel mixed with subassembly steel at its normal smeared density. The lower axial reflector is plugged with 35% of the core cladding steel. Lowering this extremely dilute fuel slug gives
rise to a reactivity change of $0.045/cm$ when the slug reaches the puddle upper surface. The puddle surface was chosen for the reactivity measurement to provide the maximum gain for this form of reentry.

Reentering fuel may fall into the core in varying degrees of compaction. The slug may be diluted with steel or it may simply contain a large amount of void. If 3 rings of fuel (15 subassemblies) fall coherently, the effect of fuel slug dilution can be seen in Table III.

**TABLE III**

The Effect of Fuel Slug Dilution on Reactivity Gain for Three Subassembly Ring Coherent Reentry

<table>
<thead>
<tr>
<th>Slug length $L$ (cm)</th>
<th>Slug fuel density ($\text{full density = 8 gm/cc}$)</th>
<th>$\sigma$/cm ($$/cm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>20</td>
<td>8.0</td>
<td>1.70</td>
</tr>
<tr>
<td>53</td>
<td>3.0</td>
<td>0.78</td>
</tr>
<tr>
<td>104</td>
<td>1.5</td>
<td>0.45</td>
</tr>
</tbody>
</table>

The cases in Table III all reflect a coherent slug of fuel falling toward a large puddle of fuel. The fuel slug in the first case ($L = 20$) is full density fuel. In the other two cases the density of fuel is reduced by extending the slug length and lowering the fuel density accordingly. The reactivity gain $\sigma$/cm appears to be approximately proportional to slug density.

In another form of noncoherent fuel reentry, material may fall into the core in a staggered fashion as shown in Fig. 6. This pattern of reentry may actually occur in 3-dimensional fashion, at random positions over the puddle. The 2-dimensional representation has been chosen for calculational convenience.
This general type of incoherent reentry may result from the timing and spatial variations that occur in the initial upward dispersions. The fuel slugs in Fig. 6 are identical with those in Fig. 5 except each ring is displaced axially by 10.6 cm. With no staggering of the rings, the reactivity gain is 1.21/cm. This is higher than the corresponding 53 cm slug of Table III due to the reflective properties of the steel in the slug (see Table II also.) If the rings of fuel are staggered as shown in Fig. 6, the reactivity gain reduces to 0.29/cm. This rather marked effect suggests that it is the degree of bulk fuel coupling with the puddle which determines the reactivity gain. The bulk of the slug fuel is substantially displaced at prompt criticality and the reactivity gain is therefore reduced. In the calculations above, we have assumed a rather dense puddle of fuel. Another incoherent effect may arise if the puddle is substantially less dense. This situation may arise if sufficient time is available for violent fuel boiling. Void fractions as high as 30\% may exist in nucleate boiling. If frothing occurs, void fractions may be quite large. Reduction of puddle density will reduce estimates of reactivity gain from slug-puddle recriticality and is another example of effects which may mitigate ramp rates.

Hence, incoherence effects can have a rather large influence on the reactivity gain (\$/cm) from fuel falling toward a puddle. If the range of reactivity gains discussed above for coherent and incoherent reassembly is utilized in conjunction with the uncertainty in slug velocity at prompt critical, a very broad range of reactivity ramp rates results. For example, single subassembly reentry may lead to a reactivity gain of around 0.04/cm while a fully coherent 5 ring reentry may lead to a gain of 1.9/cm. If gravity collapse for a distance of 70 cm is assumed, the velocity at prompt critical is 370 cm/sec. Hence, depending on the type of fuel reentry, the ramp rate may range from 15/sec to 700/sec. Additionally the slug velocity at prompt critical may not reflect gravity fall at all. Pressures which act on the slug may dominate gravity and slug velocity may be substantially different from that produced by gravity. Hence, presenting upper and lower estimates of reactivity ramp rates due to reentry recriticality does not provide useful bounds on the excursions which may result. Some degree of engineering judgment must be offered, at this time, in lieu of a detailed understanding of the extended core motion problem. In the case of small reactors, we feel that some form of incoherent reentry is likely. Furthermore, we expect that vapors present in the core during reentry will limit the reentry velocity at prompt critical. Taking these subjective factors into consideration, it is our current opinion that recriticality ramp rates may be of the order of 100/sec or so. We have not made such judgments for larger LMFBR's.

B. Recriticality Below the Core

Once recriticality in the core region ceases to be a problem, some fraction of the original fuel inventory will rest on top of the lower axial reflector and shield structure. The detailed description of this structure varies among LMFBR's. While such calculations will vary from case to case, one example is provided in an FFTF-like reactor design. Post accident heat removal calculations\textsuperscript{17,18} predict that it takes from several minutes to over an hour to melt through this lower subassembly structure, depending on the degree of retention of fission products in the fuel. When meltthrough occurs, molten fuel and steel will run down toward the hemispherical lower core support structure. Since the space between the support structure and lower shield is expected to be full of sodium, fuel coolant heat transfer is expected. This interaction will tend to fragment the molten fuel and steel. The fragments may then fall into the dish-shaped support structure forming a particle bed. For relatively thick beds, sodium boiling leads to bed dryout and melting. Three potential mechanisms exist, then, for recriticality in the support dome. First, fuel and steel fragments may fall into the dome, forming a bed which eventually reaches criticality. Second, there may be insufficient fuel for the bed to be critical, but dryout and
melting could cause the bed to slump into a critical puddle. Finally, if the slumped puddle is not critical the steel mixed in the puddle may rise due to buoyancy and form a reflector on top of the puddle, inducing criticality. Alternatively, large chunks of shield structure may eventually drop onto the puddle, enhancing the reflection from above. Neutronics calculations were made on the geometries assumed below using the TWOTRAN transport code in R-Z geometry and 16-group Hansen-Roach cross section set.

In the first calculation, an attempt is made to predict the minimum amount of fuel required for criticality in the dome. The model shown in Fig. 7 was devised so as to conserve the volume of the dome. It was assumed that the stainless steel separates from the molten fuel and settles on top of the fuel to form an upper reflector. The liquid fuel density was assumed to be 8.7 gm/cm³ with atom fractions appropriate to a homogenized core in its normal configuration. That is, atom densities were computed by volume averaging the atom densities in the two core enrichment zones. We compute a critical height of 15.47 cm, corresponding to a fuel mass of 954 kg or 33.5% of the original inventory.

If the fuel is diluted with steel and void and is not well reflected above, larger amounts of fuel are required for criticality. Assuming that only 40% of the total fuel inventory can stably reside in the core region as a puddle,¹² we conclude that criticality in the dome is unlikely until a configuration similar to that shown in Fig. 7 is reached. If the reflector above the fuel region in Fig. 7 is changed from steel to sodium, the critical mass is increased to around 50% of the total inventory. Since the fuel which initially falls into the dome will be diluted with steel fragments, we do not expect criticality immediately following meltdown of the shield. However, since there are no alternative escape routes, the total fuel inventory which remains in the core region following the disassembly(s) will eventually fall into the dome. Criticality, if it occurs, may then result from particle bed dryout, melting, and steel flotation. The reactivity ramp rate one might expect from this event remains to be assessed.

If permanent in-place cooling of core debris cannot be demonstrated, we must consider the case in which the debris eventually reaches the reactor vessel
bottom. Again, if particle bed dryout cannot be precluded, a configuration similar to that shown in Fig. 8 may arise. TWOTRAN-II was used in this study.

Four-group cross sections for each of the three regions, and appropriate to the compositions and temperatures assumed, were generated by the IDX code. For the geometry shown in Fig. 8 (total fuel inventory) the calculated $k_{\text{eff}}$ was 0.947. The radial neutron leakage was negligible but the top and bottom leakages were 16.9 and 30.7%, respectively, of the total neutrons produced. In order to examine the sensitivity of $k_{\text{eff}}$ to changes in fuel height, two additional problems were run with fuel volumes corresponding to 83 and 110% of the total inventory and a total height (fuel + steel) of 34.02 cm. A transport calculation was also made to determine the critical fuel height assuming slab geometry with vessel wall and steel thickness as shown in Fig. 8. Results of these calculations are summarized in Table IV. It is seen from Table IV that $k_{\text{eff}}$ does not vary rapidly with fuel height - an increase of 1% in fuel mass raises $k_{\text{eff}}$ by 0.0026. Comparing the R-Z and slab problems for the same fuel height, we observe that the slab calculation overestimates $k_{\text{eff}}$ by about 10%.

### TABLE IV

Criticality of Molten Fuel in Bottom of FFTF Vessel

<table>
<thead>
<tr>
<th>Geometry</th>
<th>Fuel height (cm)</th>
<th>% of total fuel</th>
<th>$k_{\text{eff}}$</th>
</tr>
</thead>
<tbody>
<tr>
<td>R-Z</td>
<td>14.02</td>
<td>100</td>
<td>0.947</td>
</tr>
<tr>
<td>R-Z</td>
<td>12.83</td>
<td>83</td>
<td>0.903</td>
</tr>
<tr>
<td>R-Z</td>
<td>14.75</td>
<td>110</td>
<td>0.973</td>
</tr>
<tr>
<td>Slab</td>
<td>12.82</td>
<td>-</td>
<td>1.000</td>
</tr>
</tbody>
</table>

We conclude that the fuel collected at the bottom of the vessel as a molten layer will not achieve criticality. However, the collection of fuel on the vessel bottom might build up as a bed of steel and fuel particulates immersed in a pool of sodium. The criticality of this configuration remains to be assessed.
V. Some Disassembly Calculations for Postulated Secondary Bursts

We have seen that a wide class of secondary neutronic excursions may result from a given accident initiator such as the unprotected loss of flow accident. Uncertainties in large scale material dynamics currently force us to include a rather broad range of secondary excursions in the overall accident delineation. Improved methods will allow us to make more precise calculations in the future and to better resolve the overall accident consequences. A few classical disassembly calculations can be performed on postulated recriticality events in order to gain some insight into the potential consequences. The results for secondary excursion disassembly calculations vary slightly from calculations of initiating bursts because a larger fraction of the core is already molten before the second burst.

Reactivity ramp rates may range anywhere from a few $/sec for incoherent reassembly to a few hundred $/sec for a sodium vapor driven reassembly. If more than one subassembly is involved and coherence is assumed, the rates may be correspondingly higher.

One example of the range of consequences one might expect from recriticality involves the 3 ring slug reentry configuration of Fig. 3. This semi-coherent postulated reassembly mechanism (0.14 $/cm) may give rise to reactivity ramp rates ranging from 0 to $400/sec depending on the slug velocity at prompt critical. Slug velocities depend on the very uncertain details of the interaction of sodium with hot slug materials. The range 0-400 $/sec was chosen with the intent to provide heuristic guidance in judging the consequences of postulated recriticality events as a function of reactivity ramp rate.

VENUS-II was used to analyze the disassembly of the assumed configuration for 50, 200 and 400 $/sec ramps. Power densities were taken directly from the DIF2D edit of the diffusion theory calculation of $eff in the prompt critical configuration. Real and adjoint 7 group fluxes were saved and a two-dimensional perturbation theory calculation was performed to obtain the material worth distribution for input to VENUS-II. Other point kinetics parameters such as delayed neutron data, prompt neutron generation time and precursor concentrations were taken from the corresponding SAS-2A unprotected flow coastdown calculation. Other data taken from SAS included fuel temperatures in rings 4, 5, and 6. Uniform temperatures of 3200°K were assumed for rings 1-3 fuel. A total doppler coefficient of \( \frac{1}{T} \frac{dK}{dT} = -0.0024 \) was weighted according to the regionwise fuel mass fraction. All material densities and volume fractions were taken directly from the corresponding DIF2D data. In order to reflect the rigidity of the plugged lower axial shield and radial reflector, a 12% void space was used in the VENUS-II reflector equation of state. In the upper axial reflectors, the void space from the DIF2D data was used directly. Hence, there was 86% void space above rings 1-3 and 44% void space above rings 4-6, where the inconel reflector was still in place. The voided region between the reentering slug and puddle in Fig. 3 was treated as reflector material containing 89% void space. All 3 cases include substantial amounts of void space throughout the entire core since the core is completely sodium voided and because a good deal of void space is introduced from homogenization of sodium voided control rod and loop subassemblies.

The results of the three calculations are summarized in Fig. 9. The average core temperatures were obtained by fuel mass fraction weighting of the estimated regionwise core temperatures. It is seen that the average core temperature varies between 4000 and 8000°K as the ramp rate increases to 400 $/sec. Hence, the condition of the core at the end of the secondary burst can vary greatly depending on the severity of the burst. The manner in which a given excursion loads the reactor vessel is the subject of other studies and will not be discussed here.
VI. Permanent Dispersion of Core Material: the Path to Quiescence

In early studies of hypothetical accidents, the accident sequences were generally terminated by vigorous disassembly events. Since the pressures generated during the disassembly excursions were often thousands of atmospheres, there was little doubt that a gross dispersal of the fuel would be achieved. As the mechanistic treatments of the initiating phase have been further developed, excursions that range down to near zero must be considered. The pressures driving the fuel dispersal can be as low as a few atmospheres.

A careful analysis of the extended fuel motion during a dispersal is important in terms of analyzing recriticality. It is also needed to describe how the fuel is ultimately dispersed into a permanently coolable and subcritical geometry.

Following a general disruption of the core, a large fraction of the fuel must be removed from the original core region to insure continuing subcriticality. The exact nature of this dispersal depends strongly on the reactor design. A few general comments will be given here with regard to an FFTF type design. A more detailed discussion can be found elsewhere.4

In FFTF, the initial dispersal should be predominantly upward. This is expected to be true for both an energetic disassembly event or a more gradual, low pressure dispersal. Even though the ejection of molten materials through the upper subassembly structure is resisted by freezing, plugging and possibly mechanical jamming, the above core structure is less massive and impervious than the radial and below core structure. Once fuel is ejected into the above core sodium pool, it is expected to particulate (if it isn't already in the form of vapor or small particles) and be broadly dispersed by the motion of the sodium. Although some of this fuel could ultimately reenter the core region, most of it should come to rest on the above core structure or in the outlet piping. If it can be shown that this fuel settles into a permanently subcritical and coolable configuration, then it can be excluded from further criticality considerations.
A fraction of the fuel may remain in the core region and eventually meltdown through the lower subassembly structure due to decay heat. The time scale for this melt-out has been estimated to be on the order of from tens of minutes to hours. Some preliminary analyses being done at ANL indicate that upward fuel removal should decrease the mass of fuel in the core to a subcritical level on a time scale that is short compared to this melt-out time. The exact position in the below core structure where this remaining subcritical mass comes to rest is determined by the post-accident heat removal analysis.

VII. Uncertainties and Suggested Key Research Areas

The exact sequence of events following a postulated HCDA such as an unprotected flow coastdown accident is extremely difficult to predict. Voiding, slumping and prompt burst predictions are quite sensitive to uncertain material worth data. Good agreement between experimental and calculational values for fuel, steel, and sodium worths are extremely difficult to attain. C/E values for fuel worths may be as high as 1.4. Steel C/E worth values are, typically, ~1.2. Sodium worths are particularly difficult to calculate and C/E values vary from 0.5 to 0.9, depending on measurement type and location in the assembly. The uncertainties in these worths give rise to uncertainties in the initial conditions for weak dispersion material dynamics. Further uncertainties in the modeling of the early stages of the HCDA complicate the analysis. The influence of fission gas in mid and end of cycle fuel is not thoroughly understood. Prediction of the precise motion of molten clad and fuel after the sodium voiding stage is also difficult.

The uncertainties in the weak dispersion phase of the LOF HCDA magnify the uncertainties in the early stage. Research is needed in the area of extended core material dynamics, including an assessment of the thermal and mechanical response of the above core structure. Particular attention should be paid to the disposition of fuel from the time at which it becomes mobile until it is at rest and coolable. Secondary criticality may complicate extended core motion and hence must be carefully monitored. For many distorted core geometries, transport theory neutronics calculations may be necessary. Variations in the neutron energy spectrum during massive core rearrangement will cause cross section variations. Proper treatment of the cross sections is important in determining the exact state of the system should secondary criticality occur. If sufficient care is taken in the use of existing reactor physics codes, uncertainties in the neutronic aspects of HCDA analysis can be reduced to a relatively insignificant level.

The interaction of molten sodium with molten fuel or steel is an important aspect of LMFBR HCDA analysis. Such interactions are important during many phases of a given accident since pressures generated are likely to influence core material motion and therefore can strongly affect the course of events. Experimental and analytical studies are needed to better understand the result of slug type interactions above and below the core. The modes of mixing and heat transfer are uncertain as well as the motion of the fuel-steel material during and following the interaction.

Finally, uncertainties in the equations of state of fuel, steel, and sodium are much more important in analyzing extended core motion than in classical disassembly analysis. Multi-phase equation of state information up to and beyond the critical temperature of these materials may be needed. Thermodynamically consistent equations of state must be developed to facilitate their use in computation.
VIII. Summary and Conclusions

Two credible mechanisms for secondary criticality within the core barrel region of small LMFBR's include reentry of fuel which has been ejected into the upper plenum and slumping of fuel which remains in the core region and is inadequately cooled. Emphasis has been placed on reentry recriticality in this paper. In the unprotected loss of flow HCDA molten steel relocation tends to block off exit paths for fuel dispersal from a mild disassembly. Molten fuel will tend to freeze and plug on its way up the plenum even if flow passages are initially unblocked. Hence the core tends to become temporarily bottled up. Since coolant passages are obstructed, fuel continues to melt and thus becomes mobile. This condition encourages recriticality. Reactivity ramp rates calculated for these postulated core compactions vary widely. This wide band of ramp rates is the result of substantial uncertainty in extended core motion calculations. A good deal of noncoherence is likely in reentry recriticality, however, and we expect reactivity ramp rates to be limited by this effect.

XI. Acknowledgments

The authors wish to acknowledge the work and help of staff members at ANL and LASL who contributed to this paper. Dr. M. Battat of the LASL performed some of the TWOTRAN calculations. Many of the DIF2D calculations were performed by Dr. W. L. Woodruff of ANL. Dr. W. A. Bezella of ANL also contributed to the DIF2D studies. General information on the predisassembly phase of the unprotected loss of flow HCDA was provided by the Initiating Accident Section of the Reactor Analysis and Safety Division of ANL.

The work presented in this paper has been sponsored by the USAEC.
Appendix I - Reactor Physics Data Used in Recriticality Analysis

1. Reactor Model

Most information utilized in the recriticality calculations is described directly, or is inferred from Ref. 6. Some of the more relevant information provided in that report is duplicated here for convenience (see Tables V - VII).

### TABLE V

Fresh Fuel Number Densities (70°F)

<table>
<thead>
<tr>
<th>Isotope</th>
<th>Inner core zone 22.43 w/o Pu/(U + Pu)</th>
<th>Outer core zone 27.37 w/o Pu/(U + Pu)</th>
</tr>
</thead>
<tbody>
<tr>
<td>U-235 (0.007)</td>
<td>0.000003996</td>
<td>0.00003750</td>
</tr>
<tr>
<td>U-238 (0.993)</td>
<td>0.00566857</td>
<td>0.00531926</td>
</tr>
<tr>
<td>Pu-239 (0.86465)</td>
<td>0.00142015</td>
<td>0.00173664</td>
</tr>
<tr>
<td>Pu-240 (0.11691)</td>
<td>0.00019202</td>
<td>0.00023481</td>
</tr>
<tr>
<td>Pu-241 (0.01669)</td>
<td>0.00002741</td>
<td>0.00003352</td>
</tr>
<tr>
<td>Pu-242 (0.00175)</td>
<td>0.00000288</td>
<td>0.00000352</td>
</tr>
<tr>
<td>Oxygen*</td>
<td>0.01440794</td>
<td>0.01443589</td>
</tr>
</tbody>
</table>

* Oxygen number density based on 1.96 E(U + Pu)

### TABLE VI

Subassembly Ring Decomposition

<table>
<thead>
<tr>
<th>No. of driver fuel assemblies</th>
<th>No. of control assemblies</th>
<th>No. of test loop assemblies</th>
<th>Total no. of assemblies</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>1</td>
<td>0</td>
<td>1</td>
</tr>
<tr>
<td>2</td>
<td>5</td>
<td>0</td>
<td>6</td>
</tr>
<tr>
<td>3</td>
<td>9</td>
<td>3</td>
<td>12</td>
</tr>
<tr>
<td>4</td>
<td>15</td>
<td>0</td>
<td>18</td>
</tr>
<tr>
<td>5</td>
<td>18</td>
<td>6</td>
<td>24</td>
</tr>
<tr>
<td>6</td>
<td>28</td>
<td>0</td>
<td>30</td>
</tr>
</tbody>
</table>

Slightly different reactor models are used in some of the calculations in the text, but all models are based on FTR type reactors. Different models were used because the calculations presented herein have been performed over a long period of time and in different national laboratories.

### Neutron Cross Sections

The cross sections utilized in the three ring coherent reentry calculations were based upon a 30-group set originally created by D. A. Meneley and C. N. Kelber for FFTF using the ARC MC2 code. While three temperatures (1100°K, 2200°K, and 4400°K) were available in the set, only the 1100°K cross sections were used.
<table>
<thead>
<tr>
<th>Component</th>
<th>Volume fractions</th>
<th>Number densities (atoms/barn-cm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Lower axial shield</td>
<td></td>
<td></td>
</tr>
<tr>
<td>stainless steel</td>
<td>0.7460</td>
<td>0.0622179</td>
</tr>
<tr>
<td>sodium</td>
<td>0.2540</td>
<td>0.0062662</td>
</tr>
<tr>
<td>Pin bottom</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Inner core zone</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Stainless steel</td>
<td>0.4169</td>
<td>0.0347703</td>
</tr>
<tr>
<td>Sodium</td>
<td>0.5831</td>
<td>0.0143851</td>
</tr>
<tr>
<td>Outer core zone</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Stainless steel</td>
<td>0.4079</td>
<td>0.0340197</td>
</tr>
<tr>
<td>Sodium</td>
<td>0.5921</td>
<td>0.0146071</td>
</tr>
<tr>
<td>Axial reflectors (top and bottom)</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Stainless steel</td>
<td>0.2382</td>
<td>0.0198664</td>
</tr>
<tr>
<td>Inconel 600</td>
<td>0.3179</td>
<td>0.0283440</td>
</tr>
<tr>
<td>Sodium</td>
<td>0.4077</td>
<td>0.0100580</td>
</tr>
<tr>
<td>Void</td>
<td>0.0362</td>
<td>—</td>
</tr>
<tr>
<td>Insulator pellets (top and bottom)</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Stainless steel</td>
<td>0.2382</td>
<td>0.0198664</td>
</tr>
<tr>
<td>Sodium</td>
<td>0.4077</td>
<td>0.0100580</td>
</tr>
<tr>
<td>UO₂ (10.42 gm/cm³)</td>
<td></td>
<td></td>
</tr>
<tr>
<td>U-235</td>
<td>0.3196</td>
<td>0.0000520</td>
</tr>
<tr>
<td>U-238</td>
<td>0.0073761</td>
<td></td>
</tr>
<tr>
<td>Oxygen</td>
<td>0.0146334</td>
<td></td>
</tr>
<tr>
<td>Void</td>
<td>0.0345</td>
<td></td>
</tr>
<tr>
<td>Active core zone</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Stainless steel</td>
<td>0.2382</td>
<td>0.0198664</td>
</tr>
<tr>
<td>Sodium</td>
<td>0.4077</td>
<td>0.0100580</td>
</tr>
<tr>
<td>Fuel*</td>
<td>0.3541</td>
<td>(see TABLE V)</td>
</tr>
<tr>
<td>Gas plenum and top end cap</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Stainless steel</td>
<td>0.2943</td>
<td>0.0245452</td>
</tr>
<tr>
<td>Sodium</td>
<td>0.4039</td>
<td>0.0099642</td>
</tr>
<tr>
<td>Void</td>
<td>0.3018</td>
<td></td>
</tr>
</tbody>
</table>

*Bulk smear density = 84.6% TD
because there was some question about the accuracy of the U-238 self shielding in the other sets.* Pu-241 and Pu-242 were not available in the set, but the atom densities of these isotopes in FTR are low.

In order to reduce computational costs, the 30-group cross sections were collapsed to 7 groups using the flux volumes from a 30-group, sodium-out base case. A utility code consistent with the ARC XSISO format was used. A 7-group collapse resulted in a $1$ error with respect to the 30-group result, which was deemed acceptable for this study. The energy and fission yield breakdown for the 7-group calculations is as follows.

<table>
<thead>
<tr>
<th>Group</th>
<th>1</th>
<th>2</th>
<th>3</th>
<th>4</th>
<th>5</th>
<th>6</th>
<th>7</th>
</tr>
</thead>
<tbody>
<tr>
<td>Upper Energy</td>
<td>1.0x10^7</td>
<td>8.2x10^5</td>
<td>3.0x10^5</td>
<td>1.1x10^5</td>
<td>4.0x10^4</td>
<td>5.5x10^3</td>
<td>1.2x10^3</td>
</tr>
<tr>
<td>Chi</td>
<td>0.76</td>
<td>0.17</td>
<td>0.05</td>
<td>0.01</td>
<td>0.003</td>
<td>0.002</td>
<td>0.0002</td>
</tr>
</tbody>
</table>

Several other cross section sets were used in the other calculations mentioned in the text and a reference is provided in each case.

Cross sections were generated for the incoherent reentry calculations beginning with fine structure cross sections. These cross sectional data sets were generated using the MC2 code with the ENDF/B1 data set. A 30-group structure consisting of 26 groups with $\Delta u = 0.5$ (lethargy units), 3 groups with $\Delta u = 1.0$, and a thermal group were developed from consistent P1 problems with buckling iterations to convergence. The Pu-239 fission spectrum was selected along with approximate thermal constants. The isotopic input data was obtained from Ref. 6, and was identical to that employed in the DIF2D calculations.

Essentially, nine MC2 problems were run with 30 energy group dependent isotopic data sets obtained for the inner core, outer core and axial reflector regions at 1000\degree K. Data sets were also obtained for the heavy isotopes in the core regions at 2000\degree K. In addition, sodium in and sodium out values were obtained for the core regions at both 1000\degree K and 2000\degree K.

A 10 energy group cross section data set was obtained using the flux volumes from the 30 group set used in the reference sodium out intact FTR core. The utility code described in Ref. 26 was employed for this collapsing. The 10 groups, generated from the 30 group data, had the following number of groups (starting from high energy) 2, 2, 2, 3, 3, 3, 3, 4, 4, 4.

3. Assumptions

All calculations assume that control rods are in the fully withdrawn position. Special loops and subassemblies are volumetrically smeared in the rings since R-Z calculations are performed (in lieu of x-y calculations).

When temperatures were assumed for various regions, thermal expansion was accounted for in all material except subassembly can walls. No assessments of the doppler effect were made since fuel motion reactivity effects were large in comparison with associated doppler feedbacks.

*Personal communication with H. Hummel (ANL), who did some independent checks of doppler coefficient using this set.
Appendix II - Fuel Slug Reversal in the Upper Plenum Region Due to Sodium Vapor Generation

Assume:
- slug density = $\rho$ = constant
- slug length = $L$ = constant
- slug velocity at $t = 0$ and $z = 0 = v_0$
- subassembly walls = in strength
- slug is impermeable to driving vapors
- no drag on subassembly walls

Fig. 10. Simplified model for estimating fuel slug reversal within a subassembly.

If

$$\Delta P(t) = P_u(t) - P_D(t)$$

then

$$\Delta P(t) = \rho L z(t)$$

where

$$z(t) = \text{position of lower slug surface at time } t.$$  

If

$$\Delta P(t) = -P \text{ (constant)},$$

$$z(t) = - \frac{Pt^2}{2\rho L} + v_0 t$$

the slug stops at $z_{\text{max}} = \frac{v_0^2 \rho L}{2P}$ and has velocity $-v_0$ when passing $z = 0$ on the way down.

Assume $v_0 = 1000 \text{ cm/sec}$, $\rho = 8$ and $L = 20 \text{ cm}$. For $z_{\text{max}} = 10 \text{ cm}$ a constant overpressure of 8 atm will stop the slug in 20 msec.

Now consider $\Delta P(t) = -P_U(t'-t)$

where

$$u(t'-t) = \begin{cases} 
0 & t < 0 \\
1 & 0 < t \leq t' \\
0 & t > t' 
\end{cases}$$
then

\[ z(t) = \begin{cases} \frac{-P_l t^2}{2\rho L} + v_o t & t \leq t' \\ \frac{-P_l t'^2}{2\rho L} + v_o t' + \left( v_o - \frac{P_l t'}{\rho L} \right) (t - t') & t > t' \end{cases} \]

The slug will reach velocity \(-v\) as it passes the origin \((z = 0)\) at time \(t'\) if \(P = \frac{2\rho L v_o}{t'}\). Hence, a pressure pulse in the shape of a square well of 320 atm. lasting for 1 msec would have the same effect as a pulse of 32 atm. lasting for 10 msec. That is, a 1000 cm/sec slug will stop, turn around and attain a velocity of \(-1000\) cm/sec under either condition. If \(P\) arises from a slow, continuous vapor generation, it may have the same net effect on slug motion as a rapid vapor explosion, unless fuel freezing occurs during the longer time scale pressurization.

Core material ejected into the plenum will consist of some mixture of molten \(UO_2\) and steel. If the steel is in thermal equilibrium with fuel which is just molten (3100°K), the following simple analysis\(^\text{27}\) suggests that a vapor explosion is more likely between steel and sodium than between \(UO_2\) and sodium.

Assume molten steel or fuel at 3100°K is injected into saturated sodium at 1150°K. From Henry's correlation\(^\text{28}\) the minimum temperature for film boiling in saturated sodium at 1 atm. is less than the melting temperature of steel. If hot (H) and cold (C) fluids are brought together, the instantaneous contact temperature \(T_i\) is given by

\[ \frac{T_i - T_c}{T_H - T_i} = \left( \frac{K_H \rho_H C_H}{K_C \rho_C C_C} \right)^{1/2} \]

If the instantaneous contact temperature is greater than the spontaneous nucleation temperature, a vapor explosion can occur without delay. The spontaneous nucleation temperature of sodium at 1 atm. is around 2400°K.

If the following estimates of material properties are used, e.g.

<table>
<thead>
<tr>
<th></th>
<th>Sodium</th>
<th>Steel</th>
</tr>
</thead>
<tbody>
<tr>
<td>(K) (cal/sec-cm°C)</td>
<td>0.12</td>
<td>0.08</td>
</tr>
<tr>
<td>(\rho) (gm/cc)</td>
<td>0.74</td>
<td>7.00</td>
</tr>
<tr>
<td>(c_p) (cal/gm°C)</td>
<td>0.306</td>
<td>0.179</td>
</tr>
</tbody>
</table>

then the instantaneous contact temperature is 2430°K, and a vapor explosion may proceed without delay. In comparison, for \(UO_2\) at 3000°K and sodium at 1150°K, the contact temperature is only around 1700°K and an explosion is less likely.

While spontaneous nucleation is felt to be a necessary condition for rapid vapor production, it may not be a sufficient condition for a true vapor explosion. Liquid-liquid contact appears to be necessary as well as some degree of geometrical constraint. The entire process is currently under investigation and will be discussed in greater detail in another paper\(^\text{29}\) in this Conference.

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References


SOME PROBLEMS CONCERNING FAST REACTOR SAFETY
by D. ANTONAKAS - M. FORTUNATO - A. MEYER HEINE (CEA/Cadarache-FRANCE)
P. FRANCOIS (EDF - FRANCE)

The safety of a fast power reactor is ensured at three levels: detection, shutdown, containment.

As far as the detection of primary incidents is concerned, redundancy is the general rule. In the same way, the shutdown function is made reliable through multiple devices: the classical control rod system is divided into two groups, each of them having an independant command circuit. A different conception is even projected for each of those systems.

Turning to the containment barriers, three levels are forecast:
- the clad,
- the primary containment: primary vessel and top shield,
- the secondary containment.

A quasi permanent control of those barriers is ensured.

It is thus rather intuitive that accidents implying a simultaneous failure of the instrumentation, the shutdown systems and the containment have a negligible probability of happening.

Maybe for historical reasons, a survey is made of some accidents without any physical meaning, such as a coherent core collapse and these accidents are often taken as examples.

That is why a list of accidents covering a wide range from the most likely to the most academic, even unrealistic, is sometimes associated to fast reactors, perhaps unconsciously. The upper limit cannot be stated clearly and it is quite obvious that it is always possible to imagine and accumulate speculative conjunctions of events.

Our purpose is to treat all the probable accidents in a mechanistic way - that is to say to be able to describe the trees of events issued from any initial defect, qualitatively and quantitatively. Then, by estimating the limits of the containment, it would be possible to "locate" the vessel among all the defects classified by order of importance.

It is quite obvious that some accidents cannot be classified in this list. These are the unrealistic accidents which are not related by definition, to any known initial cause. Although these accidents must not be taken into account, it may be useful to investigate them.

In a first part, the courses of events which can be described in a mechanistic way will be analysed: classical incidents such as a blockage, or accidents which assume a simultaneous failure of two levels of protection - detection and shutdown.

In a second part, a parametric analysis of an unrealistic accident will be performed. This will be given as an example in order to illustrate the possibilities of the codes under development in the CEA.
Finally, the response of the containment devices to mechanical energy releases will be investigated.

I. ANALYSIS OF MECHANISTIC ACCIDENTS

1.1. General comments

A mechanistic analysis is exposed to a double difficulty:

In the first part, it is difficult to describe qualitatively the sequences of events issued from a primary cause. Such a description needs the knowledge of various fault-trees covering all the possible cases. But it is only through such a description that the chronology of incidents, the list of shutdown orders and the probabilities of accidents can be determined.

Furthermore, a quantitative analysis means that the models describing the behaviour of the various core components during the course of the accident have been adjusted to experimental results. Complex mathematical models are sometimes necessary to have a precise description of the phenomena.

On that point, the present tendency at the CEA is to develop simple models. Their complexity will increase progressively with the knowledge of the different phenomena. Experimental results given by in-pile (CABRI) and out-pile tests (CEA/Grenoble, EDF/Chatou) will help to adjust those models.

As the present models included in the code SURDYN are simple and deliberately conservative, the results which will be summarized in this paper must be considered as pessimistic. It is necessary to wait for more elaborate models, validated on experiments, in order to have realistic descriptions of the phenomena.

However, the present analysis carried on a reactor as SUPER PHENIX seems to indicate that initial defects may degenerate into severe accidents only if there is a simultaneous failure of the surveillance and the shutdown systems.

The accidents which are looked over at the present time can be listed in two groups:

Accidents resulting from local defects,
Accidents resulting from "global" defects.

1.2. Local defects

They are certainly the most likely causes of accidents, especially if one compares them to the global defects which will be described in the next paragraph.

a) Local blockage of subassembly

The consequences of such an accident depend strongly on the possibility of a burn-out of the clad due to sodium vaporization. Such a burn-out would appear on the part of the fuel element immediately after the blockage. Thus the size of the blockage is an important parameter.

In a first step, an experimental study of this incident is done, mainly in the SCARABEE loop inside the CABRI reactor. Three experiments have been performed up to now and their interpretation is still in progress:

- SCARABEE V: single channel geometry. The blockage was formed of a stainless steel piece blocking half of the channel. It was located at the middle of the fissile part and was two centimeters long. The linear power of the fuel was 345 w/cm at the maximum.

  Measurements have shown that no local boiling appeared; the clad was intact even at nominal power; its destruction could have been initiated only through a flow reduction.

- SCARABEE VIII: a blockage of the 5/6 th of the channel was provided at the upper part of the fuel element (2/3). The maximum linear power was 400 w/cm. No boiling was detected. The clad resisted without difficulties during one hour at the nominal power. Boiling started only after a 50% flow decrease.
The experiment was stopped before any destruction of the clad.

- SCARABEE 7-IV : this experiment has been performed on a 7 pin bundle. The six subchannels around the central pin were blocked with a 2 centimeter long piece. The linear power and the coolant flow have first been at their nominal values. The outlet sodium temperature was only 15°C higher than for a normal bundle. Boiling and clad rupture happened after a 50 % decrease of the sodium flow. Metallurgic investigations are underway; first estimations seem to indicate that the clad rupture is not very important.

This whole set of results lead to encouraging conclusions as far as safety is concerned. A detailed interpretation is in progress and should result in adjusted models of calculations.

Some more experiments are planned to enlarge our knowledge of these incidents. Possibility of detection and propagation will be the two main topics.

b) Bottom subassembly blockage

Because of the radial inlet flow at the bottom of each SUPER PHENIX subassembly, such a blockage seems totally incredible: there cannot be a simultaneous blockage of the apertures. Furthermore the detection of a progressive blockage is easy and the response time of the instrumentation allows a totally safe shutdown of the reactor.

A complete blockage at the bottom may be considered as a superior limit of a local blockage. Obviously, if this accident were to happen there would be a burn-out of the clad and fuel melting. A violent fuel/sodium interaction could be imagined then in case of a sodium reentry. Note that up to now, no experiment has ever shown such a phenomenon. Calculations reported in indicate that an eventual compaction of the core would not lead to a positive reactivity insertion. An objection still stands if the damaged subassembly is located at the outside radius of the core and if the compaction is not isotropic.

Risks of propagation to surrounding subassemblies have yet to be examined. An experimental program is under investigation; it will show if it is valid to assume large deformations of the core.

c) Clad rupture and fission gas release

Transient voiding of a limited number of channels may lead to a propagation of the phenomena. Once more, if the pessimistic aspect of the incident is considered, the consequences could be of the same order as for a local or even a total blockage of the subassembly.

Experimental studies of these incidents have been made in some foreign countries. An experimental program is scheduled on the CABRI reactor; it will be carried on 7 and 19 pins bundles.

d) Sodium leakage from the primary vessel

The reactor can be designed in such a way that the feeding apertures of the heat-exchangers are always below the sodium level even in case of an important leakage. Thus, this accident will not be described in the present paper.

I.3.- Global accidents

An important reactivity insertion due to an eventual withdrawal of the control rods or due to a collapse of the diagrid is unrealistic: too many precautionnary measures are taken as far as those components are concerned.

Special focus will be directed towards a coolant flow coastdown without any control rod shutdown. This accident is often quoted in fast reactor safety studies though it appears to be intuitively not realistic. In our opinion, such an accident must not be considered in a reactor as SUPER PHENIX, since much emphasis has been put in order to avoid it:
- **instrumentation**: the order of shutting down the reactor happens 3 seconds after the interruption of the electric supply on the pumps. Furthermore, there are two thermocouples at the top of each subassembly; they work on a two-out-of-two logic and their response time is of about 4 seconds. The scram order is transmitted at most one second after an increase of 10°C, of the outlet sodium temperature, has been detected.

- **shutdown system**: the division of the control rods into two groups of different conception has already been quoted. Note that one third of the rods is enough to bring the reactor below cold undercriticity. A complementary shutdown system which could be installed, would obviously add another degree of protection.

Furthermore, dispositions are taken to limit the consequences of such an accident:

A high inertia is provided on the primary pumps: in case of an electric coastdown, the coolant flow would have a very progressive decrease (ratio of two in 50 seconds). An emergency device must ensure a flow rate of 10% of the nominal value.

The core is not clamped. A radial movement of the subassemblies through dilatations at the pad level is then possible in case of a sodium temperature increase. This would contribute to an important decrease of the reactor power.

In spite of all those precautions, the analysis of the response of the reactor to such an accident is under way, treated as an example.

The influence of the main parameters included in the SURDYN code will be emphasized hereafter.

The principal parameter is the sodium temperature coefficient, or more exactly its component due to the radial dilatation of the subassemblies. This component depends on the sodium temperature at the pad-level. At the nominal power, the subassemblies are in contact with each other.

a) Fig. 1 shows the variation with time of the sodium flow, the power and the maximum temperature of the sodium in SUPER PHENIX. No sodium boiling appears. The increase in temperature is very progressive: 200°C after a 3 minutes delay, the power of the reactor having decreased at half of the nominal value. After 15 minutes, the sodium temperature starts to decrease because of the natural convection cooling.

b) To illustrate the importance of the "pad coefficient", two more calculations have been made (table I).

The first one supposes that the pad coefficient is 20% lower than the reference value. Sodium boiling starts after 13 minutes (fig. 2).

The second calculation assumes a zero pad coefficient, which would correspond to a clamping of the core preventing any movement of the subassemblies. Boiling starts after 2.5 minutes.

These two calculations have been carried on up to the power excursion phase due to the progressive voiding of the channels. The results obtained with the SURDYN code are certainly very pessimistic because of the models used:

- assumption is made of a total emptying of a subassembly in 200 to 400 milliseconds as soon as the boiling temperature is reached. In fact, the phenomenon is rather progressive; moreover experiments have shown that a total expulsion of the sodium would only appear 15 seconds after boiling initiation, if the subassembly is near its nominal power.

- furthermore, one assumes that every subassembly within the same ring will boil and expulse its sodium at the same time. The reactivity ramps associated with sodium ejection are thus artificially increased.

Taking into account all those pessimistic hypotheses, it becomes obvious that high thermal energy releases can be obtained at the end of the power excursion phase. (About one third of the core could melt).
Improvements on boiling models developed in connection with experimental results will lead to more limited consequences. Note, once more, that the reference case does not give any boiling phenomena.

II. - ANALYSIS OF AN UNREALISTIC ACCIDENT

The previous approach has the advantage of showing the sequences of events, when of course it is possible to describe them qualitatively and quantitatively.

One can notice that when we suppose simultaneously
- a failure of the detection systems,
- a failure of the shutdown systems,
- a pessimistic analysis in our models.

it is possible to melt a certain amount of the core.

In order to determine the influence of the various parameters which are involved in the following course of events, it might be interesting to study a completely unrealistic event which cannot be related to a primary cause. For instance a coherent core collapse under gravity.

II. 1. - Sequence of events

The accident is initiated by a coherent collapse of the fuel, happening on a core initially near to its nominal conditions.

The corresponding reactivity ramp is evaluated from the material reactivity worth and a simple slumping model ignoring any restraint. For a large reactor like SUPER PHENIX, this ramp is ranging around 60 dollars per second.

Then evaluation is given of the heat production due to the power excursion which is generated by this reactivity ramp. The amount of molten fuel is also computed.

That evaluation assumes a certain number of parameters which depend on the core conditions when the accident starts:

- Doppler effect, depending on the amount of sodium within the core.
- Fuel burn-up: whether fission products are present or not in the fuel, their thermodynamic properties may induce different ways of core disassembly at the end of the excursion.
- Density and compressibility of various materials in the core: here again, according to the amount of sodium, various types of fuel dispersion may happen, due to the smaller or larger compressibility of the core.

In the present, only the case of a voided core is considered; this hypothesis is certainly pessimistic, as the Doppler effect will be at its lowest value and the core disassembly will be less effective.

It is also supposed that the reactor has been operating at full power for 6 months before the accident, so that the presence of fission products is considered.

Following the excursion phase, the consequences of a thermodynamic interaction between the molten fuel and a given quantity of sodium are analysed. This phenomenon takes place in two phases:

First, a thermal transfer is considered between the highly dispersed fuel and the liquid sodium. According to the inertia of the surrounding "cold" sodium, the interacting zone reaches high pressures and high temperatures before vapour appears.

Then, vaporization starts and provokes a mechanical energy release whose effects on the containment structures are analysed.
A certain number of parameters are introduced in the analysis of this MFCI:

- Particle size
- Mixing time between the dispersed fuel and the sodium.
- Amount of sodium involved.
- Heat transfer law between both materials during the vaporization phase.

The influence of those parameters on the mechanical energy release will be shown below in a brief survey.

II.2.- Mathematical models

Just a brief description of the computer codes which are used will be given here.

a) Power excursion phase: the SUREX code contains the following main features:

- The core is modeled in cylindrical geometry
- Point-kinetics with 1 group of delayed neutrons
- The fuel is supposed adiabatic; its specific heat varies versus temperature. The heat of fusion is taken into account.
- The equation of state of the fuel during the disassembly phase accounts for the additional pressure generated by the fission products, associated with the vapour pressure of the mixed-oxyde.
- Reactivity changes due to material motion are calculated by first-order perturbation theory.

b) First phase of the MFCI: the code TRACON has the main following features:

- Uniform particle size.
- Constant amount of sodium within the interacting zone, with an uniform temperature.
- The amount of interacting fuel can vary linearly with time, within a given time interval.
- The heat transfer law between both materials is CHO and WRIGHT's transient conduction model. \[3\]

- The dynamic behaviour of the interacting zone is computed in one-dimensional spherical geometry. The surrounding cold sodium is supposed incompressible.

c) Second phase of the MFCI: many computer codes exist or are being developed in order to analyse the mechanical energy release connected with the hydrodynamic behaviour of the cold sodium and the dynamic response of the structures.

Concerning vapour expansion, the model BUEE contains the following assumptions:

- The heat transfer model takes into account the mixing law which has been described previously. The contact area between the fuel particles and the liquid sodium changes according to the volume of vapour in the interacting zone.
- The effect of vapour recondensation near the surrounding cold sodium can be considered, but it does not induce an effective reduction of the mechanical energy generated when the interaction concerns large amounts of fuel.

The dynamic response of the cold sodium and the containment is analysed either in spherical geometry (Codes BUEE, BOUQUET) or in cylindrical geometry (Codes ANDREAS, ORION, CRATER). Some of those codes can take into account the compressibility of the cold sodium and the effect of internal structures within the main vessel.
II.3. Some numerical results

Calculations have been done on a large LMFBR submitted to a power transient caused by a reactivity ramp of 60 dollars/second.

a) The thermal energy release during the power excursion has been computed, supposing that the reactor has been working at full power for 6 months and that the core is initially completely void of sodium.

It is also assumed that the power level is of the same order of magnitude as the nominal power. That hypothesis may appear to contradict the previous one (core voided); however calculations show that the final result does not depend significantly on the initial power, by the nominal conditions.

Table II shows the main results of the calculations concerning the power excursion.

b) The evaluation of the mechanical energy release at the end of the MFCI has been made with the following set of parameters:

- Particle size: 2 radii have been considered: 100 and 500 microns. Calculations made for zero-particles size lead to results twice higher than the 100 micron case.

- Amount of interacting sodium: the results which are presented here are obtained with an optimum coolant-to-fuel mass ratio, which is included in the range 10 to 30%, according to the case which is considered. For higher ratios, the mechanical energy does not decrease significantly (15% less when the mass of sodium is three times higher). On the other hand, this energy decreases rapidly for ratios lower than the optimum.

- Mixing time: in order to show its effect, 3 values have been considered: 0, 10 and 100 milliseconds. Beyond 10 ms, a progressive decrease appears on the mechanical energy; between 0 and 10 ms, the effect is less obvious: in some cases higher energies are obtained for 10 ms than for zero. That can be explained according to the fact that a slower duration of the phenomena may increase the amount of thermal energy transferred to the sodium until large vapour formation slows down this heat transfer. This effect appears in table III for a particle radius of 500 microns.

- The heat transfer model between fuel and liquid sodium during the second phase of the MFCI has a large influence on the order of magnitude of the results. The law which has been used here is described above. Other results have been obtained, supposing a cut off of the heat transfer when vapour appears: mechanical energies 8 to 10 times lower than those given in table III can be found when the mixing time is different from zero. A systematic investigation of this parameter is being performed in parallel with the experimental program which is achieved at the CEA/Grenoble.

III.- CONTAINMENT

The point is now to analyse the effects of an energy release on the containment structures and to locate their limits.

To do this, two complementary approaches are performed at present: computer codes and experiments on mock-ups.

III.1.- Computer codes

Only the codes in two-dimensional cylindrical geometry are described here.

The effect of the mechanical energy release on the containment is computed by one of the following codes:

- ANDREAS: hydrodynamic code, considering the sodium within the main vessel as an incompressible fluid, and using an Eulerian computational scheme. The sodium motion is correlated to the bubble expansion and to the dynamic response of the vessel, radially and axially. The vessel is considered as a thin shell. The free sodium surface can move and compress the cover gas. The
roof motion under this pressure build-up can be computed. This code cannot yet take the internal structures into account (bundle of subassemblies, core hold-down plate, internal vessel).

- ORION: hydrodynamic and elasto-plastic code using a Lagrangian mesh. The sodium is considered as a compressible liquid and the internal structures of the reactor can be treated. This code is still under development in parallel to the experimental program which will be described below.

- CRATER: this code can compute the effects of the mechanical energy release on various structures: internal vessel, diagrid, main vessel, and roof. The calculations are made analytically in both radial and axial directions in correlation with the pressure in the expanding bubble. This simplified code is used mainly for parametric calculations. The results concerning the volume increase of the main vessel and the impulse on the roof have been compared to the corresponding results obtained with ANDREAS. That comparison has been performed without internal structures. The agreement between calculations is very good.

III.2. Experiments on mock-ups

The experimental program which is performed at present on mock-ups of SUPER PHENIX (1/30 scale) has for main purposes:
- to give results on the behaviour of the containment structures when large mechanical energies are released (1000 to 3000 MJ),
- to give informations on some specific phenomena: influence of internal structures, possibility of directional effects,
- to validate the computer codes by comparing them to the experimental results.

This program will be carried on till 1976-77.

In a first step, experiments are made on simplified mock-ups: charge in a tank full of water, without internal structures. The mock-ups are then gradually completed in order to analyse separately the influence of each component.

In addition to this program, some analyses are performed on the representation of materials and complex structures, on the simulation of sodium-bubble growth by chemical explosions and on the comparison between sodium and water simulation.

A final demonstrative test will be performed on a highly representative mock-up of SUPER PHENIX.

III.3. Some preliminary results

- Calculations have been made with the help of the existing codes in order to evaluate the effects of a mechanical energy release within the reactor, without considering the influence of any internal structure.

  The results show a good behaviour of the main vessel and the roof to energies up to about 1000 MJ. However, by taking into account the reducing effects due to the internal structures, it will certainly be possible to demonstrate a good behaviour to higher energies.

  Nevertheless, the models presently included in the computer codes need validation on the expected experimental results.

- Some preliminary experiments were made with bare charges equivalent to an energy of about 1000 MJ at reactor scale.

  A very good behaviour of the tank was noticed. However, let us mention that the charge was composed of a low density chemical explosive with characteristics rather different from a vapour explosion.
CONCLUSION

The program of safety studies carried out at the C.E.A. implies an important work both theoretically and experimentally. Physical understanding of the phenomena and their description, validation of the models on experiments are the main objectives.

In order to be confident in the models involved, adjustments are made on a very large range of accidents.

Among those accidents, one considers first those which can be described in a mechanistic way and for which the reactor shutdown is always assumed. These are the accidents which must be taken into account in a safety analysis.

Then, studies are performed on some other accidents which assume a simultaneous failure of the instrumentation and the shutdown systems. It appears that some of them, which are not kept in the safety analysis - for instance, an unprotected flow coastdown - may not lead to severe consequences when some design measures are taken in the project.

In order, to be exhaustive, investigations are performed on some accidents, taken as examples, which cannot be connected to well-known primary causes. In fact, the distinction between those unrealistic accidents and the previous ones for which simultaneous defects were assumed, is somewhat artificial: their probability of occurrence is totally negligible for both of them. Anyway, the studies performed up to now show that the containment of a large fast breeder reactor would even withstand the mechanical energy released by these academic accidents.

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### TABLE I.

**UNPROTECTED FLOW COASTDOWN**

<table>
<thead>
<tr>
<th></th>
<th>Reference core</th>
<th>Pessimistic case</th>
<th>Clamped core</th>
</tr>
</thead>
<tbody>
<tr>
<td><strong>Pad coefficient</strong></td>
<td>-0.7 pcm/°</td>
<td>-0.54 pcm/°</td>
<td>0</td>
</tr>
<tr>
<td><strong>Maximum temperature of sodium</strong></td>
<td>1190° K</td>
<td>boiling</td>
<td>boiling</td>
</tr>
<tr>
<td><strong>Boiling starts at time</strong></td>
<td>-</td>
<td>797 s</td>
<td>142 s</td>
</tr>
<tr>
<td><strong>Maximum reactivity ramp</strong></td>
<td>-</td>
<td>9 $$/s</td>
<td>10 $$/s</td>
</tr>
<tr>
<td><strong>Amount of molten fuel</strong></td>
<td>-</td>
<td>13 200 kg</td>
<td>13 600 kg</td>
</tr>
<tr>
<td><strong>Thermal energy release in molten fuel</strong></td>
<td>-</td>
<td>13 700 MJ</td>
<td>14 200 MJ</td>
</tr>
<tr>
<td><strong>Average temperature of molten fuel</strong></td>
<td>-</td>
<td>2 910° C</td>
<td>2 930° C</td>
</tr>
</tbody>
</table>

### TABLE II.

**POWER EXCURSION DUE TO A COHERENT CORE COLLAPSE**

<table>
<thead>
<tr>
<th></th>
<th>-60 $$/s</th>
</tr>
</thead>
<tbody>
<tr>
<td><strong>Reactivity ramp</strong></td>
<td></td>
</tr>
<tr>
<td><strong>Doppler coefficient</strong></td>
<td>0.4 %</td>
</tr>
<tr>
<td><strong>Thermal energy deposited in the fuel</strong></td>
<td>46 000 MJ</td>
</tr>
<tr>
<td><strong>Amount of molten fuel</strong></td>
<td>27 000 kg</td>
</tr>
<tr>
<td><strong>Percentage of molten fuel</strong></td>
<td>75 %</td>
</tr>
<tr>
<td><strong>Thermal energy deposited in molten fuel</strong></td>
<td>35 000 MJ</td>
</tr>
<tr>
<td><strong>Average temperature of molten fuel</strong></td>
<td>3 830° K</td>
</tr>
</tbody>
</table>
Amount of interacting fuel: 27,000 kg
Initial temperature of the fuel: 3,830° K

<table>
<thead>
<tr>
<th>Mixing time</th>
<th>100 µm</th>
<th>500 µm</th>
</tr>
</thead>
<tbody>
<tr>
<td>0</td>
<td>3022 MJ</td>
<td>740 MJ</td>
</tr>
<tr>
<td>10 ms</td>
<td>2987 MJ</td>
<td>1731 MJ</td>
</tr>
<tr>
<td>100 ms</td>
<td>2005 MJ</td>
<td>1190 MJ</td>
</tr>
</tbody>
</table>

**TABLE III.**

**MOLTEN FUEL/COOLANT INTERACTION**

Mechanical Energy release, in Megajoules for a bubble expansion up to 450 m³
UNPROTECTED FLOW COASTDOWN
(REFERENCE CASE)

\[ P/P_0 : \text{NORMALIZED POWER} \]
\[ D/D_0 : \text{NORMALIZED COOLANT FLOW} \]

\[ T \text{ K} \]

\[ T : \text{MAXIMUM TEMPERATURE OF SODIUM} \]
UNPROTECTED FLOW COASTDOWN
(PESSIMISTIC CASE)

VOIDING STARTS IN CHANNEL : 1

NET REACTIVITY

PROMPT REACTIVITY

0.3%
0.2%
0.1%
0

NORMALIZED POWER

1000
100
10
1

TIME

MINUTES

SECONDS

NOTE CHANGE IN TIME SCALE

Fig. 2
CURRENT STATUS AND EXPERIMENTAL BASIS OF THE
SAS LMFBR ACCIDENT ANALYSIS CODE SYSTEM*

by

M. G. Stevenson, W. R. Bohl, F. E. Dunn, T. J. Heames
G. Hopchner, and L. L. Smith

Argonne National Laboratory, Argonne, Illinois 60439

1.0 Introduction

The SAS integrated accident-analysis code system has been under development for some time. The initial version, SAS1A,1 was a single-channel code which included point reactor kinetics; a combination of annular slip two-phase flow and single-bubble slug-boiling models; an elastic-plastic cladding and elastic-fuel deformation model, DEFORM-I; a transient fuel-pin heat-transfer model; and an internal switch to the MARS2 disassembly module.

The sodium-boiling model was later completely rewritten as a multibubble slug boiling model for the multichannel SAS2A version.3,4 A new fuel-pin deformation module, DEFORM-II, was also included in a version of SAS2A. DEFORM-II allows consideration of irradiated pins (restructured fuel, transient release of retained fission gas) and includes fuel plasticity in the deformation calculation. SAS2A did not include the MARS disassembly module but provided punched output as input for the VENUS-II disassembly code.6,7

The latest version, SAS3A, a preliminary version of which was reported earlier,8 includes models which allow an integral treatment of both transient overpower and loss-of-flow unprotected accident sequences through fuel-pin disruption and up to the point of subassembly disruption. In some cases, these models can follow the sequence from initiation to neutronic shutdown by dispersive fuel motion and can be used to evaluate the possibility of establishing in-place cooling. For cases which proceed to an energetic core disassembly, the new models provide an improved definition of core conditions and reactivity insertion rate at the time of a transition to VENUS-II. The new capabilities provided by SAS3A include a steady-state fuels-initialization routine, a primary-loop hydraulics model, a fuel-coolant-interaction model for transient overpower conditions, a new moving-film treatment in the multibubble sodium-boiling model, and cladding and fuel motion models for voided assemblies in a loss-of-flow accident.

The phenomenological models included in SAS3A have been, in general, developed with only a partial understanding of the mechanisms involved, and thus may be modified through further comparison with experiments. The body of relevant experimental evidence has grown rapidly in the past two years, largely through experiments performed in the TREAT reactor. The recent application of

*Work performed under the auspices of the United States Atomic Energy Commission.
the SAS3A models to in-pile tests will be summarized in this paper in addition
to a discussion of the basis of the models at the time they were developed.

The overall organization of the paper is as follows:

1.0 Introduction
2.0 SAS3A Structure
3.0 New General Modules
4.0 Phenomenological Modules
  4.1 Sodium Voiding (TSCOOL)
  4.2 Cladding Relocation (CLAZAS)
  4.3 Fuel Motion (SLUMPY)
  4.4 Fuel Pin Mechanics (DEFORM) and Failure Predictions
  4.5 Fuel Ejection and Fuel-coolant Interaction (FCI).
5.0 Future Developments

For each of these latter five modules, the following will be discussed:

(1) Model characteristics
(2) Basis for model
(3) Model evaluation with respect to recent experiments

2.0 SAS3A Structure

The major conceptual modules in SAS3A and the relationships between these
are identified in Fig. 1. In this context a conceptual module is defined as
all of the calculations pertaining to a specific model, i.e., coolant dynamics,
fuel motion, etc. In actuality, the code is made up of a series of subroutines
which do not have a one-to-one correspondence to the modules identified here.
Typically, the calculations which fall within one module are carried out by a
group of subroutines which may be entered in different ways depending on the
situation. Thus, this chart represents the phenomenological structure of the
code rather than the actual computational structure.

The top line of Fig. 1 consists of the modules used in the steady-state
initialization process. There is an iterative loop among these modules such
that the fuel pin characterization and the thermal-hydraulics are self-
consistent. The second line shows the transient modules which formed SAS2A
and these allow a detailed treatment up to the point at which fuel pin in-
tegrity is lost. The new modules provided in SAS3A are shown in the-bottom
two lines. Figure 1 does indicate some of the restrictions on SAS3A as well
as the capabilities. At the present time the cladding and fuel motion modules
cannot operate simultaneously in the same channel with the FCI model, although
all can operate in a partially voided channel if the location of the initial
pin failure is in a nonvoided region of the channel.

The "pin failure" check following the DEFORM box may be somewhat mislead-
ing, although loss-of-pin integrity is required before proceeding to the FCI
or CLAZAS modules. In actuality, there is no cladding-failure criterion based on the strains calculated by the fuel-mechanics module built into the code, and instead checks are made on thermal conditions or on a cladding strength comparison with hoop stress from either plenum fission-gas pressure (for loss-of-flow cases) or internal pressure in the fuel region due to transient release of fission gas.

Through the transient modules there are several iterative loops which are not shown in Fig. 1. For example, there is an iterative loop between TSHTR and the transient point kinetics module, TSPK, in order to converge the reactor power with the temperature-dependent reactivities prior to beginning the remaining calculations. There are several levels of time steps in the transient calculations, with the longest being the heat transfer time step. Nested inside it is the primary loop time step which in turn encompasses the coolant dynamics time step. Depending on the situation, FCI or SLUMPY time steps may nest inside the coolant time step.

3.0 New General Modules

3.1 Fuels Characterization

In addition to geometry, hydraulics, and physics input, an important part of the initialization process in the SAS3A code is the determination of the initial conditions for irradiated fuel. Radii of the restructured fuel zones, fuel swelling, cladding swelling, and fission-gas release and retention are among the necessary information that must be input or calculated before the initiation of a transient calculation. The SAS2A code included the capability for treating restructured fuel, but providing the input without a direct link to a fuels code such as LIFE-II was a time-consuming and inefficient process. For this reason, a simple "fuels-characterization" routine, SSFUEL, was added such that restructuring calculations can be made directly and, in addition, correlations for fuel swelling, cladding swelling, and fission-gas release and retention are provided.

In these calculations, the restructuring of the fuel is based upon restructuring temperatures at each axial node for equiaxed- and columnar-grain fuel. The restructuring temperatures can be input constants (isothermal region boundaries) or can be axial- and channel-dependent as determined by a burnup dependent correlation. Fuel swelling is included simply by using an input volumetric swelling rate (linearly proportional to burnup) for each structural region. The restructuring and swelling calculations are performed in the same iteration as the steady-state heat-transfer calculations, so that the resulting fuel temperatures and structural-region boundaries are consistent. The calculational algorithm re-positions the radial temperature mesh so that the structural-region boundaries fall on a mesh boundary. Care is taken to preserve the fuel mass at each axial node. The nominal FFPE clad-swelling correlation is provided in the calculations.

The fission-gas release model is an updated version of the model of Dutt, et al., in which the fraction of gas released is given by a linear heat rating- and burnup-dependent correlation.

The released fission gas is added to the plenum and central void where it is mixed with the amount of fill gas determined from the plenum volume and input plenum pressure at the reference temperature. In order to determine the amount of gas in each of these regions, it is assumed that the plenum gas and the central void gas are in pressure equilibrium.
3.2 Primary Loop Hydraulics

In both loss-of-flow and transient-overpower accidents, hydraulic coupling between coolant channels has a direct bearing on the development of core-voiding patterns and, in many cases, on subsequent cladding and fuel motion. For example, in the voiding phase of a loss-of-flow accident, flow diversion from voiding subassemblies delays boiling initiation in cooler subassemblies. To account for these effects the PRIMAR module was developed and included in SAS3A. Although the main objective was to provide hydraulic coupling between coolant channels, compression of cover-gas volumes and a treatment of primary-loop hydraulics was also included. The PRIMAR module is directly coupled with the coolant dynamics module (TSCOOL). However, the coupling is numerically explicit at the inlet and exit to each channel rather than implicit or iterative; thus, solution techniques in each module remain independent of each other. PRIMAR includes inlet and outlet coolant plenums which are connected to coolant channels and a bypass channel (control-rod and radial-reflector subassemblies for example). The primary-loop module provides inlet and outlet plenum pressures to the coolant-dynamics module, which returns detailed channel mass flow rates as input for the primary-loop calculation. The outer primary loop contains a pump, intermediate heat exchanger, and piping. The pump pressure history is presently supplied by input. The present version also does not allow for unbalanced-loop effects or single loop pipe rupture calculations. A new version will be developed to remove these limitations in the near future.

4.0 Phenomenological Modules

4.1 Sodium Voiding

The sodium-boiling model used in SAS3A began its development as a new version of the single-bubble slug-boiling model included as an option in SAS1A. The new version was similar to the models of Cronenberg and Fauske\(^1\) and of Schlectendahl\(^2\) and included an explicit calculation of vaporization of a liquid film remaining on pins after voiding and of condensation in colder regions, and vaporization or condensation on the liquid interfaces. This model was appropriate for situations involving high superheat.

However, early calculations with superheats as high as 70°C using the single bubble model showed that the lower liquid slug heated well beyond saturation, indicating that additional bubbles should be formed. This model was then modified to allow a new vapor bubble to form in the liquid coolant at any point where the liquid temperature exceeded the local time-dependent saturation temperature by more than the original superheat criterion. It was also observed that, because of the high rate of vapor production and the resulting vapor velocities, large axial pressure gradients should exist in a large vapor bubble. For this reason the model was modified to account for the pressure gradients, and the resulting multibubble model was included in the standard version of SAS2A.

This model thus allows the formation and continued presence of a number of bubbles in a channel and follows the bubbles either as they move up a channel and collapse (or reach the exit), or as they become sustained and void the channel. Small bubbles are actually treated with a uniform pressure slug-ejection model similar to the original model, including vaporization (condensation) from the surfaces. The sodium vapor and liquid film are assumed to be saturated. As a vapor bubble grows past a predetermined length (typically 5-10 cm), the pressure distribution in the bubble is calculated, and the calculation of interface vaporization (condensation) is dropped since the exposed cladding surface area far exceeds the axial interface areas.
Although this sodium-boiling model was based on previous models which were in turn based on an understanding of boiling phenomena obtained from out-of-pile experiments, the first direct experimental check of the model was provided by a calculation\(^{15}\) of an out-of-pile flow-blockage experiment reported by Peppler\(^ {16}\) of Karlsruhe. In this case, the calculations and experiment were in reasonably good qualitative agreement with respect to flow rate oscillations and initial voiding behavior. However, the dryout of the film was based only on vaporization, and the experimentally observed dryout time was much shorter than the calculated time. For this reason it became necessary to consider an additional dryout mechanism, i.e., stripping of the film by streaming vapor as described by Grolmes and Fauske.\(^ {17}\)

To account for vapor stripping and film draining, a one-dimensional annular film-flow model\(^ {18}\) has been developed as an integral part of the SAS2A boiling model for inclusion in SAS3A. The motion of the liquid film is calculated explicitly under the influence of vapor drag, wall friction, gravity, and the axial pressure gradient. Droplet entrainment is not considered.

The film-dryout time predicted by this model for the single-pin type of sodium boiling experiment of Peppler agrees well with the experimentally observed dryout time. The new model predicts film dryout at 0.47 sec after the start of boiling, whereas Peppler observed film dryout at 0.48 sec after the start of boiling.

Results of the film-motion model for a loss-of-flow transient are shown in Fig. 2. This figure shows the extent of the voided region and the film thicknesses calculated at five points in time after boiling inception. At boiling times of 0.24, 0.34, and 0.44 sec, there are a number of separate vapor bubbles and liquid slugs in the coolant channel, whereas at 0.49 sec a single void was established. The film-thickness histograms at boiling times of 0.54 and 0.64 sec make it apparent that the liquid film has been carried upward from the upper part of the core by the upward vapor flow. The lower part of the relatively cold fission-gas plenum region of the pin is also being stripped of film.

In the SAS2A analyses of the Peppler experiment, it was observed that assuming dryout at two-thirds the original film thickness agreed with the experimental observations. Thus, most recent SAS2A calculations with the static-film model have used a minimum film thickness equal to two-thirds of the initial film thickness. A comparison of LOF calculations indicates that the two-thirds approximation agrees fairly well with the film-motion model as to film dryout times and cladding temperatures soon after dryout. However, results of the two models diverge after film dryout and, by the time the cladding melts, the differences are significant in terms of downward dryout development.

To verify further the boiling model, a pretest-analysis was made of the TREAT R5 seven-pin LOF experiment\(^ {19}\) and was used as part of the experiment design. The calculation of R5 was repeated following the experiment, with the only change being the use of the actual power and flow transients measured in the experiment. A comparison of the calculated and experimental results are shown in Ref. 19. Initial flow reversal and the oscillating behavior of upper and lower liquid slugs are well represented by SAS. In addition, experimental and calculated coolant temperatures at the top and bottom of the active fuel agree well into boiling, thus indicating that the overall void-growth behavior is predicted well.

While flow reversal and initial test-section voiding are predicted well by the SAS calculation, the inception of voiding is predicted to occur about 0.5 sec after that indicated in the experiment. This relatively slight variation could possibly be explained by small uncertainties in the measured
power and flow transients. However, it is perhaps more reasonable to assume that this is due to local variations in radial coolant temperatures and that these variations become averaged out once sustained voiding occurs.

Although the agreement between averaged quantities is fairly good, there is no evidence that the early voiding actually exhibits a multibubble regime in which the vapor bubbles fill the channel area. While the multibubble approach is perhaps phenomenologically correct for a single subchannel at uniform temperature, it may not be correct for situations involving local variations in temperature since initial bubbles may not fill the channel. Nevertheless, for the seven-pin test section in the R5 experiment, it adequately predicted overall voiding behavior.

For much larger test sections or for full-subassembly reactor situations, a larger degree of radial temperature variation and noncoherence of boiling initiation might be expected. With wire-wrapped fuel pins, significant noncoherence of boiling initiation would be expected even in a uniform power assembly since the outer rings of pins are considerably overcooled. Also, large radial power gradients exist in many subassemblies in any system, and this will also cause noncoherence of boiling initiation. The effect of noncoherent initiation on gross voiding behavior is not yet known; however, the voiding induced power increase in a large LMFBR should result in increased voiding coherence. Noncoherent voiding is under intensive investigation, and new voiding models to treat these effects must be and are being developed.

In addition to sodium boiling, SAS3A contains the model for voiding by plenum fission-gas release used in SAS2A. This model calculates the expulsion of the gas into the coolant channel and the resulting voiding for situations involving release into a nonvoided channel. The model is very similar to that of Chawla.  

Early analyses of a loss-of-flow accident without scram in the FFTF reactor used very conservative estimates of plenum-fission-gas pressures and irradiated-cladding strength. As a result, pin failure and plenum-gas release were calculated to occur prior to voiding. Later calculations with nominal plenum pressures and updated cladding strengths consistent with those obtained in tube-burst tests at HEDL have indicated that the pins will not fail until after sodium boiling. Since the cladding stays near the sodium saturation condition as long as the film remains on the cladding, the pins will not fail until after cladding dryout. At the present time, release of plenum gas into a fully voided channel is expected to have only a slight effect on the overall voiding behavior. It is important to note that there were no direct observations of gas release in the TREAT L3 and L4 irradiated-pin LOF tests. However, there was a noticeable difference in flow rate oscillations (chugging) as compared to the similar L2 fresh-pin test, and this has been suggested to be due to the presence of the noncondensible fission gases.

4.2 Cladding Relocation

In analyzing the loss-of-flow accident for FFTF it was recognized that, under the expected relatively constant power conditions, cladding would melt some 2-3 sec prior to fuel melting in the first subassemblies to void. Thus, although there was no direct experimental evidence of such an effect, it was reasonable to assume that the molten cladding would relocate, possibly moving out of the heated region, freezing, and plugging. The first supposition was that the cladding would drain downward and plug below the fuel region. However, following a suggestion by Fauske, it was recognized that upward sodium velocities could be high enough to cause flooding (wave formation), and
the resulting high friction drag would lead to upward cladding motion, similar to the sodium-film stripping effect. This could then cause initial cladding plug formation above the fuel region, followed by draining of later cladding to melt and plugging below the fuel region.

In addition to the mechanical effect on sodium voiding and fuel dispersal, cladding relocation has an important reactivity effect and a significant thermal effect since removal of the cladding heat sink allows fuel temperatures to rise rapidly. Thus, in order to analyze cladding relocation, the CLAZAS module\textsuperscript{25} was developed for inclusion in SAS3A.

This model uses an explicit calculation of the motion of molten clad consistent with the sodium-vapor-pressure-gradient model. The physical picture is one of discrete clad segments which can combine by moving over other clad segments. Each segment moves under the influence of gravity, the channel pressure gradient, the frictional drag due to streaming sodium vapor, and friction between moving clad and the fuel pin. Feedback effects are included to modify the coolant-channel sodium-vapor friction, the coolant channel hydraulic diameter, and the sodium vapor flow area. Heat-transfer effects are calculated by including a relationship describing heat transfer from moving clad to stationary clad, a relationship describing heat transfer from stationary fuel to moving clad, and a variable viscosity for molten clad as a function of internal energy. Since the viscosity is assumed to become very high as the cladding internal energy decreases through the heat of fusion, the cladding stops as it freezes.

Figure 3 shows results from a typical calculation by the CLAZAS model. The particular example is an analysis of the cladding motion in the L2 TREAT experiment,\textsuperscript{26} in which the fueled region extends from 10 to 45 cm. The initial cladding to melt rises under the influence of the streaming sodium vapor. These cladding nodes begin to impede the vapor flow, and the next nodes to melt drain down since the force of gravity now dominates. Downward motion appears to be primarily limited by the rate of cladding melting in the lower part of the pin. The final cladding configuration is predicted to be a fairly massive blockage below the fueled region and a lesser blockage above the fueled region.

Since the model uses relatively large segments, the detailed predictions of the model should not be expected to agree precisely with the experiment. However, these blockages are generally in qualitative agreement with the cladding plugs evident in the postmortems of the test sections. More recent out-of-pile experiments\textsuperscript{27} also indicate through direct observation that, in the sodium vapor flow regime of interest in FFTF, the film motion model of cladding flow is qualitatively correct.

Results from the R5 TREAT experiment will provide a test of the cladding motion model validity under more prototypic conditions (fuel pins in the R-series were full-length FTR prototypic pins, whereas the L2 pins were 13 in. long to correspond to the EBR-II irradiated pins used in L3 and L4). The R5 test was oriented specifically to the cladding-relocation problem and was a LOF test in which the TREAT power transient was terminated following full test section voiding and extensive cladding melting, but prior to significant fuel melting. Posttest radiographs indicate a substantial cladding blockage near the bottom of the fuel column. Later posttest examinations will be necessary to indicate the extent of any upper cladding blockage since the radiographs do not clearly show any. For both R4 and R5 there appears to be excellent consistency between the calculations and data on overall voiding behavior and on initiation of cladding or fuel motion.
4.3 Fuel Motion

A common and quite reasonable assumption in many fast reactor accident analyses has been that fuel, driven only by gravity, slumps as it melts. This was demonstrated at HEDL by Weber et al.\textsuperscript{28} in fresh fuel melting experiments performed in a laboratory apparatus with an externally heated fuel column. Attempts have been made to include an effective viscosity to slow the free-fall process and to calibrate the effective viscosity with experimental information.\textsuperscript{29} Following this, the first fuel slumping model used in conjunction with the SAS codes assumed that the first fuel to melt flowed as a viscous film over intact pin stubs. However, the viscosity did not seem to have an appreciable effect, since completely molten fuel is almost inviscid.

On the other hand, some overpower type experiments have indicated\textsuperscript{30} that release of retained fission gas could have a significant effect on fuel motion and possibly lead to a dispersal of fuel by fission gas. In addition, the first irradiated-pin experiment in the out-of-pile fuel melting tests performed by Weber et al.,\textsuperscript{28} indicated that, under slow heating conditions, low-power gassy irradiated fuel could be dispersed rapidly by release of retained fission gas.

Since this kind of effect could be a significant factor in an unprotected loss-of-flow accident and since there appeared to be a need for an explicit model for fuel motion in a voided channel, the SLUMPY module\textsuperscript{31} was developed for SAS3A.

SLUMPY provides a compressible-hydrodynamics calculation of fuel motion under the influence of gravity, sodium vapor, fission gas, friction, steel vapor and fuel-vapor driving forces. It can be used either to supply detailed initial conditions to a VENUS-II two-dimensional disassembly calculation, or it can be used directly as a disassembly code within the limitation of one-dimensional motion (implying intact subassembly geometry). In most SLUMPY calculations, fuel motion is assumed to be initiated when melting begins in unrestructured fuel. This assumption is based on two fuel characteristics. First, the equi-axed region is expected to be structurally strong, and initiation of motion may require that this region be near melting. Second, the unrestructured fuel should have fission gas on grain boundaries providing a fragmentation mechanism when this fuel melts. Early irradiated UO\textsubscript{2} fuel pin meltdown experiments\textsuperscript{32} in a transparent dry capsule indicated that pins would stay intact at least until incipient melting, and the more recent R- and L-series experiments also seem to verify this latter assumption.

As the fuel melts, individual axial fuel segments join the "slumped" region treated in the SLUMPY calculation. Unmelted fuel above the slumped region can fall into or be pushed out of this slumped region. The unmelted pin below the slumped region is assumed to be stationary. Both the upper and lower solid fuel segments restrict the area available for the slumped region. Axially limiting boundaries for the slumped region are dependent on user input; or, if CLAZAS is used, the boundaries may be determined by the time-dependent positions of calculated clad blockages.

The equation of state is composed of six parts. First, there is an ambient background pressure assumed to be fission gas released from the pin prior to initiation of slumping. Second, any additional fission gas released from the fuel either at slumping initiation or during fuel motion adds to this background pressure. Slip is included between gas and fuel particles. Third, the sum of the fuel vapor pressure and stainless-steel vapor pressure (if present) replaces the background fission-gas pressure if such vapor pressures are large enough. Fourth, if a transition to a single-phase liquid occurs, single-phase pressures will replace the vapor or fission-gas pressures. Fifth, the local ambient
background pressure can be increased to the sodium vapor pressure predicted by the boiling model if the channel is not plugged. In this case a high friction factor is fed back to the boiling model to simulate the effect of fuel. Sixth, a pseudo-viscous pressure\textsuperscript{7} is added to any calculated pressure to help provide more numerical stability.

A detailed study of gas release and swelling has been carried out for inclusion in SLUMPY calculations, using a bubble coalescence model to represent the fission-gas behavior. The calculations were carried out using a relatively small computer code that has evolved from studies begun nearly ten years ago.\textsuperscript{33} The code is currently called FRAS (Fission-gas Release And Swelling) and is similar to the GRASS code.\textsuperscript{34}

Since the FRAS model is approximate and the major parameters are not well-known, the gas-release calculations have been calibrated\textsuperscript{35} with two experiments which had quite different heating conditions. The first of these is the TREAT H3 failure-threshold experiment\textsuperscript{36} and the second is the HEDL out-of-pile fuel-melting experiment FGR-15.\textsuperscript{28} The calculations with FRAS give only the release of gas to the grain boundaries. Additional assumptions must be made to describe the effect of the gas on initial fuel motion, since models do not presently exist to describe either the gas release from grain boundaries to the coolant channel or the fragmentation of the fuel due to grain-boundary gas. At the present time, the particular assumptions used lead to the formulation of a fuel-particle suspension in melting irradiated fuel. If reactor power is high, melting fuel will tend to disperse since fission gas is being released from fuel faster than it is being lost via slip. If the power is low, fission-gas slip will dominate and the fuel will tend to collapse. At steady-state power a dispersion will tend to be maintained until either steel or fuel vapor pressures build up. This latter possibility is consistent with L3 and L4 fuel motion results where little axial fuel motion was observed until the possible generation of steel or fuel vapor pressures, although the gas-particle suspension possibility is only one of several plausible scenarios.

There are several experiments, most of which have been alluded to earlier, which have a significant bearing on the SLUMPY modeling of fuel motion. First, some of the out-of-pile experiments performed by Weber et al.,\textsuperscript{28} indicated that fresh fuel columns heated from the outside would slump or fall rapidly as they melted. The fresh-fuel L2 loss-of-flow experiment in TREAT also showed fuel slumping, but much more slowly than expected from free fall.\textsuperscript{26} This can be duplicated in SLUMPY by varying the viscosity as the fuel goes through the heat of fusion. However, it may be more likely that the fuel motion in L2 was a complex process involving crumbling of pellet stacks (once cladding was removed) and asymmetric power distributions in the test section. A relatively simple single-pin model like SLUMPY cannot adequately analyze this situation. Although the model can be forced to fit the experiment, the mechanisms are not presently identified clearly enough to allow extrapolating to another system.

As mentioned earlier, the first melting experiments with irradiated fuel at HEDL indicated a rapid dispersal of fuel by release of retained fission gas. These experiments were with low-power irradiated gassy fuel. Later experiments with higher-power fuel did not indicate such a dramatic dispersal, although turbulent fuel motion was evident. The later experiments, particularly FGR-15, were extremely valuable in providing transient data on gas release. Also, in a SLUMPY analysis of FGR-15, the fission-gas-fuel-particle slip assumption used in SLUMPY led to a good estimate of the length of time fuel was in motion in the experiment.\textsuperscript{37}

The L3 and L4 EBR-II irradiated-pin loss-of-flow experiments\textsuperscript{26} in TREAT have also provided some important experimental information. Both of these experiments exhibited the "eructations" of fuel seen after slumping in L2.
However, in L3 and L4 there was little previous slumping as compared to that observed in L2. At the present time, there is no definitive explanation of the reason for the difference in the fresh- and irradiated-pin tests although it should be noted that the L2 pins were completely fresh with no restructuring while the L3 and L4 pins had fully developed equiaxed fuel zones. The SLUMPY calculations can be made to match the general behavior in L3 and L4 by using the appropriate fuel particle size, viscosity, and fission gas release to form a fuel-gas suspension after initiation of motion. However, this mechanism is somewhat sensitive, and slight variations in the assumptions can cause the fuel to either rise, fall, or move in both directions.

The eruptions seen in all three experiments also raise questions regarding steel vapor and fuel vapor, since either or both of these, perhaps adding to fission-gas pressures, are plausible driving forces. Steel vapor is the more pleasing explanation since considerable steel mixing with molten fuel occurred in the experiments as evidenced by posttest examinations and since significant steel vapor pressures are expected at the temperatures thought to exist. Again, SLUMPY calculations can be made to fit the observed eruptions by making the proper assumptions and mixing in steel with the fuel.

Figure 4 shows some results from a SLUMPY calculation of the L4 experiment. The plots show the "slumped" region between and overlapping the remaining pin stubs. Here the shaded line density is directly proportional to the fuel density. In these calculations the cladding was assumed to remain on the fuel (a thin film of remaining cladding may be physically reasonable or the spacer wires could remain after cladding melting) and was mixed in with the fuel after motion initiation. The heat transfer assumptions were such that steel vapor pressures begin moving fuel at about the time of the observed eruption. In this case the final configuration of the fuel is qualitatively similar to that observed in posttest neutron radiographs.

The F-series experiments soon to be performed in TREAT should provide clearer data on fuel motion mechanisms. These tests have been designed specifically as single-pin dry-capsule tests with a heated wall in order to provide a clean separation of effects. In addition, direct electrical-heating experiments are being performed by Roberts at ANL, which will allow a better definition of fuel-motion initiation under various heating conditions for both fresh and irradiated fuel.

An important phenomenon is that of freezing and plugging as fuel is dispersed into colder regions. It is quite evident from the in-pile experiments that this should be expected in mild dispersals. Explicit modeling of plugging dynamics is being developed as part of the transition phase effort and will be included in later versions of SLUMPY.

4.4 Fuel Pin Mechanics and Failure

The SAS1A and SAS2A codes both contained the DEFORM-I fuel pin mechanics module. This module treats the cladding as perfectly elastic-plastic and the fuel as perfectly elastic, and does not allow an explicit treatment of fuel restructuring or fission gas release. The DEFORM-II module was developed to remove some of the limitations in DEFORM-I. Currently DEFORM-II is contained in a special two-channel version of SAS2A used primarily for analyses of experiments and has been included in a version of the COBRA code. It is not included in either the standard release version of SAS2A or in SAS3A.

DEFORM-II was used to precalculate the H3 irradiated-pin failure-threshold experiment in TREAT; this calculation showed about 0.3% plastic deformation in the test, indicating that the pin should not fail. The latter
turned out to be an accurate prediction, since the pin did not fail; however, posttest measurements indicated no plastic deformation within the approximately 0.2% strain measurement error. Although the 0.3% plastic deformation could be explained as due to slight errors in either analysis, initial pin characterization, experimental energy inputs, or measurement, it seems likely that the one-dimensional plane strain approximation used in DEFORM-II could easily overpredict the effective radial thermal expansion of the fuel after cladding-fuel contact.

A DEFORM-II analysis\textsuperscript{42} of the later H5 experiment in TREAT did predict a plastic hoop strain at the experimentally determined failure time which was in good agreement with burst tests\textsuperscript{43} on irradiated cladding from the PNL-17 pins used in both H3 and H5. However, a simpler "burst criterion"\textsuperscript{44} including pressurization from released fission gas similar to that proposed by Stuart and Thomas\textsuperscript{45} also gave very good agreement with the H5 failure time.

The primary emphasis of codes such as DEFORM-II has been on predicting cladding strain due to thermal expansion of the fuel against the cladding. As discussed by Culley et al.\textsuperscript{46} thermal expansion is only one of the possible mechanisms leading to pin failure. For fresh pins, meltthrough of the cladding is the likely mechanism for failure in mild overpower transients, although pressurization by fuel vapor and fill gas can cause a burst-type failure in very rapid transients in which the sodium remains subcooled, as in the TREAT S11 and S12 tests.\textsuperscript{47} With irradiated pins, transient fuel swelling due to intragranular fission-gas-bubble precipitation and coalescence can add to the thermal-expansion loading of the cladding. Perhaps more importantly, transient release of intergranular gas can lead to a loading on the cladding either directly or through essentially strengthless fuel. The latter is the mechanism assumed in the burst criterion mentioned above, and this criterion is included as an option in the FCI module in SAS3A. The phenomena involved in transient gas release are not well modeled as yet, but the FRAS code mentioned above or the GRASS code could provide a basis for this along with a model for grain boundary breakup.

Any or a combination of the loading mechanisms mentioned above can lead to irradiated-pin failure. Single-pin failure-threshold experiments are being performed by HEDL in the TREAT reactor, and these in conjunction with continued ANL H- and E-series experiments\textsuperscript{36} should provide a good experimental base for a failure-threshold correlation. However, a detailed model combining a consistent treatment of all mechanisms is needed.

\subsection*{4.5 Fuel-Coolant Interaction}

Early analyses of a mild overpower transient, HCDA in FFTF assumed that fuel pins failed at the midplane, and plenum fission gas could cause rapid voiding, leading to a prompt-critical transient and core disassembly.\textsuperscript{48} Since it was recognized that this mechanism was unrealistic, later modeling concentrated on developing a more consistent scenario for the phenomena following fuel-pin failure. The FISFAX model\textsuperscript{49} was used in an evaluation of a sodium-in prompt critical transient in FFTF and was the first to include the effect of sodium-induced (FCI) dispersive fuel motion in terminating rapid transients. This concept, along with more realistic estimates of fuel-failure locations, went into the models used in later FFTF analyses.\textsuperscript{46} These models indicated that a fuel-sweepout mechanism could terminate mild reactivity transients well before reaching prompt criticality. The SAS/FCI module\textsuperscript{50} in SAS3A includes this concept; however, both it and the improved modeling in the MELT codes were originally developed with the idea of providing a more realistic, but still conservative, estimate of the reactivity insertion rates as initial conditions for core-disassembly calculations. In this case, the early accident termination possibility came into place after model development was already partially completed.
As of yet, there is little direct experimental evidence which positively confirms the modeling in the SAS/FCI module. The H5 TREAT experiment\textsuperscript{36} appears to represent the same general kind of phenomenology, since a relatively sharp pressure spike with a following train of oscillations occurred at the initial pin failure, although the oscillations may not have been due directly to fuel ejection. Further, the neutron radiographs indicate a small, but significant, amount of fuel dispersal up above the fuel column. A direct comparison of analysis with experiment results has not yet been completed, but this is underway with the SAS/FCI module, the MELT-III code, \textsuperscript{51} and the still more detailed PLUTO model, \textsuperscript{52} which is to be included in a later version of SAS.

The overpower transient experiments\textsuperscript{36} E6 and E7 in TREAT have indicated that, if the transient is continued well beyond initial fuel failure, continued fuel melting and motion may lead to massive fuel plugging as the fuel moves out of its original position. This phenomenon is not modeled explicitly in the current codes and will need to be included in later versions, since it may have an important effect on permanent termination of the accident with in-place cooling of disrupted subassemblies.

5.0 Future Developments

Several of the limitations of the present SAS code system have been noted. In addition to upgrading, further internal checkout, and experimental verification of the existing models, the most significant needs for initiating-accident-phase modeling are: (1) Treatments of multiple-pin (noncoherent) effects on voiding and on later cladding and fuel motion, (2) a consistent and integrated treatment of fuel pin mechanics incorporating fission-gas-induced fuel swelling and fission-gas release, and (3) an integrated treatment of sodium boiling, cladding motion, and fuel motion involving thermal and hydraulic interactions among all materials under conditions appropriate to accidents in large LMFBRs.

Obviously, each of these is a complex physical and modeling problem. Further, it is likely that an accident-analysis code incorporating models of this complexity in one integrated 20-30 channel calculation (as might be appropriate for a large system) would be impractical with present-generation computers except for benchmark problems. The next-generation of phenomenological models will probably be used primarily for performing studies from which the information will be fed into the multichannel accident analysis codes. Thus, the multichannel codes must concentrate on providing a means to use this information effectively to provide a consistent accident analysis.

This may be a somewhat simpler task in the future, since as larger LMFBRs become of greater interest and sodium-void reactivities tend more to dominate the accident sequences, the initiating accident codes may revert more to their original purpose of supplying input conditions to disassembly codes. However, extended transition-phase\textsuperscript{38} analyses still will be needed for many situations, and this must become an area of increased modeling attention.

In addition, another area that is now being developed rapidly is the coupling of the SAS code system with the space-dependent kinetics code FX2.\textsuperscript{53} It is recognized that, as calculations are extended into situations involving gross material displacements, the present point kinetics with first-order perturbation reactivities may be inadequate. An initial automated but nonintegral coupling of the codes will allow consistent checks to be made of the current first-order perturbation calculations. This will also provide a good basis for designing an optimal integration of the two codes.

In summary, the models in the current SAS3A code are receiving a thorough checkout with experimental information. Near-term experiments are planned which
will allow a better definition of mechanisms in order to complete the verification of the present single-pin models. New models will be necessary to include multiple-pin effects and these must be verified by experiment. In the near-term, the emphasis will be on the experimental verification of the existing models and on attempting to incorporate information from detailed models in a consistent manner. A natural spin-off from this will be a stronger base for providing the mechanistic understanding necessary to develop probabilistic accident analysis methodology. Probabilistic accident analyses may and should become the ultimate approach provided a firm mechanistic base can be established.

In another area, the subassembly models used in SAS3A must be coupled with the models being developed to describe accident phenomena in extended transition phase analyses.\(^3\)

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**References**


47. M. Epstein and D. H. Cho, "Fuel Vaporization and Quenching by Cold Sodium; Interpretation of TREAT Test S-11," these proceedings.


Figure 1  SAS3A Module Relationships
Figure 2  Upward Film Motion and Dryout (0.64 seconds after Boiling Initiation) from SAS3A/TSCCOOL Calculation

Figure 3  CLAZAS Calculation of L2 TREAT Experiment Showing Cladding Blockage Formation at Top and Bottom of Fuel Column
Figure 4  SLUMPY Calculation of LA TREAT Experiment Showing Eruption Induced by Steel-vapor Pressure
A COMPARISON STUDY AND ANALYSIS OF MODELS FOR 1000 MWE AND 4000 MWE LMFBR HYPOTHETICAL ACCIDENTS

Paul B. Bleiweis*, David Okrent, and William E. Kastenberg
Energy and Kinetics Department
School of Engineering and Applied Science
University of California
Los Angeles, California 90024

ABSTRACT

A 1000 Mwe and two 4000 Mwe LMFBR's were analyzed for unprotected loss-of-coolant flow and reactivity insertion accidents using a version of the SAS-VENUS computer code. For similar accidents, the final energy deposited in the molten fuel increased nearly directly proportional to the core mass. A four zone, more highly flattened 4000 Mwe reactor yielded between 5% and 15% more energy in molten fuel than a two zone reactor for the same accident conditions. The energy yields were relatively insensitive to the assumed values of sodium superheat. The computer code was found to introduce potentially large errors because of the manner in which reactivity insertion rates were selected for the disassembly phase at the time of switching to the VENUS module (at a pre-specified peak core fuel temperature). Further attention should be given to this aspect of coupled-code accident analyses.

*Now at Nuclear Engineering Program, University of Illinois at Urbana-Champaign, Urbana, Illinois 61801
I. INTRODUCTION

Current safety analysis methods used to analyze LMFBR hypothetical core disruptive accidents are usually characterized by the following pattern: an initiating event (such as pump failure, flow blockage, or control rod ejection) is postulated; the accident is then traced from the initiating event up until the vicinity of prompt-criticality (with high fuel temperatures and pressures) is reached; a disassembly calculation is then performed to determine the state of the core (e.g., energy yield, mass of molten fuel, and temperature) resulting from the disassembly. Most current methods attempt to take a conservative approach within a chosen accident path. In other words, assumptions are made concerning the initiating event, the included effects, and the point at which disassembly is expected to occur; and the accident is then calculated following the set path, assuming conservative values for the important parameters (e.g., Doppler and sodium void reactivities and ramp rates during disassembly). However, a drawback to this approach is that the accident path, itself, may not be conservative. For example, a computer model which explicitly calculates the "correct" time to switch from a predisassembly to a disassembly calculation is not available today. Thus, the safety analyst must specify, in some way, when this is going to occur without having previous knowledge of the course of the accident. If a disassembly calculation is to begin when prompt-criticality is reached, the fuel temperatures and vapor pressures may be unrealistically low for a disassembly to occur. If a switch to a disassembly calculation is made too late, pressures and temperatures may be too high, or ramp rates may be too low, so that results other than those expected from the calculation may be obtained. For this and similar reasons, it may be necessary to consider alternate accident sequences.

The calculations presented in this paper were performed primarily to compare the behavior in postulated accidents of a typical 1000 Mwe LMFBR to that of two 4000 Mwe cores designed explicitly for this study1. A second purpose was to study a number of different accident paths. For example, comparison calculations were performed for large and small values of sodium superheat in an effort to determine whether any anomalies can occur because of the choice of superheat, as well as to look at the sensitivity of like accidents to variations in superheat. Also, switches from predisassembly to disassembly calculations were made at three or four points in time (corresponding to different fuel temperatures) so that a determination of the uncertainties in the initiation of a disassembly calculation could be assessed.

The predisassembly calculations presented in this paper were performed with an early version of the SAS2A2 computer code. The output of this code is then used as input for a disassembly calculation, which is performed by the VENUS-II3 computer code.

All the calculations were performed assuming beginning-of-life fuel and thus neglected the effects which result from the presence of fission gas. A fuel-coolant interaction module was not included in the versions of the SAS-VENUS codes, nor was a fuel-slumping module. Also, the sodium voiding module did not treat the thickening, thinning, or removal of liquid film from cladding and structure under the influence of gravity and shear forces due to streaming vapor, after boiling occurs. The degree of sodium superheat was chosen to be either 10°C or 100°C for all of the transients studied.

In order to determine the effects and uncertainties of switching from the SAS2A to the VENUS-II calculations, a number of different temperatures were chosen as the switch points. The first switch was selected to occur when the hot fuel node temperature reached 3500°C. At this point much of the core is
below melting (2767°C). This probably represents a good lower bound for the disassembly condition. The next switch temperature chosen was 5000°C, at which point much of the core is probably molten. This represents a more severe case. An upper temperature of 6000°C was chosen for the last switch point.

Finally, one of the main problems with the SAS-VENUS system which was studied, and one which will be discussed in more detail, is the way in which SAS2A calculates reactivity ramp rates to be used as input for the disassembly calculations. SAS2A may predict a disassembly ramp rate which is either unreasonable in sign or in magnitude at a specified switch point, since sodium voiding is a complex phenomenon with oscillating reactivity effects. For this reason, most of the VENUS-II calculations were performed using maximum reactivity ramp rates which were calculated by hand from the output of SAS2A. For the unprotected transient overpower calculations performed, all the initial disassembly ramp rates were calculated by hand. In most of the unprotected loss-of-flow calculations, both the reactivity ramp rates calculated by SAS2A and the maximum ramp rates calculated by hand were used for the disassembly calculations.

Table I indicates some of the important parameters calculated and used as input for the three cores studied. The four enrichment zone large core permitted a study to be made of the effects of a high degree of radial power flattening.

II. SUMMARY OF RESULTS

A. Unprotected Loss-of-Flow Accidents

All the loss-of-flow predisassembly calculations exhibit power and reactivity oscillations around prompt-critical which result from the competition between the negative Doppler and positive sodium void reactivity effects. The relative magnitudes of the power and reactivity oscillations, near prompt-critical, are larger for the 4000 Mwe cores than the 1000 Mwe core since the magnitudes of both the Doppler and sodium void reactivity worths of the 4000 Mwe cores are larger than for the 1000 Mwe core. This is important because smaller oscillations in the 1000 Mwe core represent less of a reactivity change with time. Thus, smaller reactivity ramp rates were present at each of the VENUS-II temperature switch points for the 1000 Mwe core. However, it is not clear when ramp rates will be negative or positive so that a switch to a disassembly calculation at 6000°C might have a smaller ramp rate than a switch at 3500°C.

The larger sodium void effect in the 4000 Mwe cores tends to shorten the time from boiling initiation to disassembly switch, as compared to the 1000 Mwe core, during the loss-of-flow accidents. Larger reactivity insertions resulting from voiding in the 4000 Mwe cores cause power and temperatures to increase more rapidly and allow the disassembly switch points to be reached earlier in time than for the 1000 Mwe core. Larger ramp rate insertions of reactivity occur in the 4000 Mwe cores due to this shortened time scale. Thus, in terms of reactor safety, the disassembly calculations, which are sensitive to initial ramp rates, appear to be relatively more severe for the 4000 Mwe cores than for the 1000 Mwe core. The effect is lessened somewhat by the fact that more voiding takes place (due to the longer time scale and the shorter core height of the 1000 Mwe core than the 4000 Mwe cores) in the 1000 Mwe core so that reactivity ramp rates as large as $177/\text{sec}$ were calculated for this core. While this ramp rate is approximately $80/\text{sec}$ less than the maximum 4000 Mwe ramp rate, it is still extremely large and indicative of a strong excursion.
Table I. Reference Core Parameters

<table>
<thead>
<tr>
<th>Core Geometry</th>
<th>1000 Mwe Core</th>
<th>Two Zone, 4000 Mwe Core</th>
<th>Four Zone, 4000 Mwe Core</th>
</tr>
</thead>
<tbody>
<tr>
<td>Active Core Height (cm)</td>
<td>109.7</td>
<td>127.0</td>
<td>127.0</td>
</tr>
<tr>
<td>Zone 1 radius (cm)</td>
<td>73.5</td>
<td>155.6</td>
<td>118.3</td>
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<tr>
<td>Zone 2 radius (cm)</td>
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<td>230.3</td>
<td>168.1</td>
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<tr>
<td>Zone 3 radius (cm)</td>
<td>---</td>
<td>---</td>
<td>205.4</td>
</tr>
<tr>
<td>Zone 4 radius (cm)</td>
<td>---</td>
<td>---</td>
<td>230.3</td>
</tr>
<tr>
<td>Height-to-Diameter</td>
<td>0.53</td>
<td>0.28</td>
<td>0.28</td>
</tr>
<tr>
<td>Axial Blanket Thickness (cm)</td>
<td>38.1</td>
<td>30.5</td>
<td>30.5</td>
</tr>
<tr>
<td>Radial Blanket Thickness (cm)</td>
<td>34.8</td>
<td>24.9</td>
<td>24.9</td>
</tr>
<tr>
<td>Fission Gas Plenum Length (cm)</td>
<td>130.1</td>
<td>163.8</td>
<td>163.8</td>
</tr>
</tbody>
</table>

| Thermal Hydraulics            | ~2500         | ~10000                  | ~10000                   |
| Thermal Power (Mwt)           | 625.0         | 567.0                   | 565.0                    |
| Sodium Outlet Temperature (°C)| 168.0         | 163.0                   | 161.0                    |
| Core ΔT (°C)                  | 1.12          | 1.06                    | 1.01                     |
| Radial Peak-to-Average Power  | 1.31          | 1.43                    | 1.03                     |
| Zone 1                        | ---           | ---                     | 1.11                     |
| Zone 2                        | ---           | ---                     | 1.26                     |
| Zone 3                        | 1.22          | 1.25                    | 1.10                     |
| Zone 4                        | 1.28          | 1.28                    | 1.28                     |
| Core Average Power (Mwt)      | 0.003297      | 0.003175                | 0.003167                 |
| Prompt Neutron Generation Time, A (sec) | 0.328x10^-6 | 0.674x10^-6 | 0.673x10^-6 |
| Doppler Coefficient (Tdk/dT)  | -0.0067       | -0.0088                 | -0.0088                  |
| Na-in                         | -0.0040       | -0.0041                 | -0.0041                  |
| Na-out                        | 3.84          | 5.67                    | 5.94                     |
| Void Worth ($)                | 4.59          | 6.38                    | 6.68                     |
| Sodium Void Worth (no blankets) ($) | 1325          |
Another important size effect is illustrated by the amount of energy deposited in the molten fuel at the end of each disassembly calculation. This represents the amount of energy released if all the molten fuel were suddenly solidified. It appears that the difference in energy in molten fuel between the 1000 Mwe and the 4000 Mwe cores is directly proportional to the amount of fuel (volume) in the core, being approximately four times larger for the 4000 Mwe cores than the 1000 Mwe core. These energies and average fuel temperatures at the end of disassembly are shown in Tables II and III for the 1000 Mwe and the four zone, 4000 Mwe cores.

An effect which is important to potential LMFBR design is the difference in the accident consequences caused by flattening the radial power profile of a core. The SAS-VENUS calculations indicate that a more highly flattened core exhibits a more coherent sodium voiding pattern, larger generated ramp rates, and a somewhat more severe disassembly during the loss-of-flow accidents studied.

The effects of superheat variations on the loss-of-flow transients studied were consistent with the expected results. Boiling initiation in the hottest channel occurs approximately 0.60 second earlier in the 1000 Mwe core, for the 10°C superheat case, than for the 100°C superheat case. Boiling initiation occurs approximately 0.50 second earlier in the four zone, 4000 Mwe, 10°C superheat case, than for the 100°C superheat case. The resulting sodium voiding progresses more slowly for the 10°C superheat cases since less excess energy has been deposited in the sodium prior to formation of the initial bubbles. The 10°C superheat accidents take longer to reach the high temperatures for disassembly switch than the 100°C superheat cases, since the time rates of change of the sodium void reactivity additions are smaller for the low superheat cases than the high superheat cases. However, maximum ramp rates for the disassembly calculations are only a few dollars per second different for the 10°C superheat cases than for the 100°C superheat cases. It appears that once sodium voiding has established itself in these cores, the course of the accidents is similar, regardless of the degree of superheat. It has been shown that for higher power density cores, such as those studied in this paper, the degree of sodium superheat is less important to the course of an accident than for cores with lower power densities. It would be expected that loss-of-flow accidents with mid-range values of superheats (approximately 50°C) would exhibit behavior somewhere between the 10°C and the 100°C superheat cases.

It is not a simple matter to determine the effects of switching to a disassembly calculation at the three values of the hot fuel node temperatures chosen for these studies, since ramp rates and initial reactivity levels are not monotonic from one switch point to the next. However, a generalization can be drawn. The higher initial temperature cases take less time to disassemble after the switch temperature is reached since initial pressures are higher. Also, the final energies in the molten fuel for these high initial temperature cases are larger than for the lower temperature cases. It is very difficult, from the results presented, to predict when a real disassembly would occur during these transients. However, using a variety of switch points, as done in these studies, enables one to determine some of the limits of the disassembly calculations.

B. Unprotected Overpower Transient Accidents

Unprotected overpower transients of $1/sec, $10/sec, and $100/sec initial reactivity ramp rates were calculated for the 1000 Mwe and the two 4000 Mwe cores for both a 100°C or a 10°C value of sodium superheat. Since the SAS2A and VENUS-II models employed for these studies do not include a calculation of
molten fuel-coolant interactions, any quantitative comparisons of energy yields, shown in Tables II and III, and disassembly switch effects are subject to modification.

There were several inconsistencies in the disassembly ramp rates calculated by SAS2A for all of the overpower transients studied. These are due to the different effects of sodium voiding on the calculations. For example, the $100/sec transients exhibit smaller input disassembly ramp rates than the other cases at the same disassembly temperature switch points. No voiding occurs in the $100/sec transients, and these ramp rates are due mostly to a combination of the positive input reactivity insertion and the negative Doppler effect. When sodium boiling initially occurs, in the $1/sec and the $10/sec transients for both values of superheat, it takes time for the void patterns to establish themselves. This adds less reactivity during the course of either transient. The most sodium void reactivity added occurs in the $1/sec cases, which take longer to reach the high switch temperatures and have more time for steady void patterns to establish themselves.

Final energies in the molten fuel again, as in the loss-of-flow calculations, appear to be proportional to the core volumes, with energies for the 4000 Mwe core being approximately four times larger than the corresponding energies in the 1000 Mwe core, as shown in Tables II and III. The mass of molten fuel for the 4000 Mwe core probably accounts for this energy increase being, in most cases, four times larger than the molten fuel mass of the 1000 Mwe core. Final temperatures, at the end of disassembly, which are dependent on the input conditions and the state of the core, appear to be, on the average, higher for the four zone, 4000 Mwe core than the two zone, 1000 Mwe core.

C. Graphical Portrayal of Results

Some results for typical unprotected loss-of-flow accident calculations are presented in Figures 1, 2, 3, and 4. Figure 1 shows the 1000 Mwe and the two 4000 Mwe predisassembly power histories calculated for a $100^\circ C$ value of sodium superheat. Indicated on the figure are the disassembly switch temperatures. Figures 2, 3, and 4 show the sodium voiding profiles at the $350^\circ C$ disassembly switch point for the 1000 Mwe and the four zone, 4000 Mwe, $100^\circ C$ superheat cases and for the four zone, 4000 Mwe, $10^\circ C$ superheat case, respectively. These figures illustrate the power oscillations, the more coherent sodium voiding pattern of the power flattened core as compared to the two zone, 1000 Mwe core, and the larger volume percentage of sodium voiding in the 1000 Mwe core as compared to the four zone, 4000 Mwe core.

Figures 5, 6, 7, and 8 show some typical results for the unprotected reactivity insertion accidents. Figures 5, 6, and 7 show the 1000 Mwe and the four zone, 4000 Mwe predisassembly power histories for the $1/sec, 10$/sec, and $100$/sec, $100^\circ C$ sodium superheat calculations, respectively. Figure 8 shows the 1000 Mwe and the four zone, 4000 Mwe predisassembly power histories for the $1/sec, 10^\circ C$ sodium superheat calculations. Indicated in these figures are the points of disassembly switch and sodium boiling initiation.

III. CONCLUSIONS

It was found that the energy deposited in the molten fuel at the end of the disassembly calculations, using SAS2A-VENUS, was approximately four times larger for the 4000 Mwe cores than for the 1000 Mwe core (for corresponding predisassembly-disassembly switch temperatures). The effect of radial power flattening was found to increase the magnitudes of the generated disassembly reactivity ramp rates due to more coherent sodium voiding effects. Also, the
### Table II

1000 Mwe Core Disassembly Results  
(100°C Superheat, Loss-of-Flow and Overpower Transients)

<table>
<thead>
<tr>
<th>SAS2A-VENUS Switch Temperature (°C)</th>
<th>Case</th>
<th>Ramp Rate at Switch Point ($/sec)</th>
<th>Final Energy in Molten Fuel (Mw-sec)</th>
<th>Mass of Molten Fuel at Termination* (Kg)</th>
<th>Final Average Core Temperature (°K)</th>
</tr>
</thead>
<tbody>
<tr>
<td>3500</td>
<td>Loss-of-Flow (SAS2A ρ/sec)</td>
<td>61.00</td>
<td>7.25x10³</td>
<td>12,800</td>
<td>3500</td>
</tr>
<tr>
<td></td>
<td>Loss-of-Flow (Max ρ/sec)</td>
<td>177.00</td>
<td>11.5x10³</td>
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<td>4250</td>
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<tr>
<td></td>
<td>Overpower Transient ($1/sec)</td>
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<td></td>
</tr>
<tr>
<td></td>
<td>Overpower Transient ($10/sec)</td>
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<td>4.8x10³</td>
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<td>3100</td>
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<td>Overpower Transient ($100/sec)</td>
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<tr>
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<td>Loss-of-Flow (Max ρ/sec)</td>
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<td>11.9x10³</td>
<td>14,000</td>
<td>4900</td>
</tr>
<tr>
<td></td>
<td>Overpower Transient ($1/sec)</td>
<td>118.00</td>
<td>10.9x10³</td>
<td>12,800</td>
<td>4750</td>
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<td>Overpower Transient ($10/sec)</td>
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<td>6000</td>
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<td>14,200</td>
<td>5500</td>
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<td>Loss-of-Flow (Max ρ/sec)</td>
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<td>12.8x10³</td>
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<td>5600</td>
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<td>5450</td>
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<td></td>
<td>Overpower Transient ($100/sec)</td>
<td>54.00</td>
<td>11.0x10³</td>
<td>13,400</td>
<td>5400</td>
</tr>
</tbody>
</table>

*Total Fuel Mass (excluding radial blanket) = 24,000 Kg  
*SAS2A sign error (should be negative ρ/sec)
Table III

Four Zone, 4000 Mwe Core Disassembly Results
(100°C Superheat, Loss-of-Flow and Overpower Transients)

<table>
<thead>
<tr>
<th>SAS2A-VENUS Switch Temperature (°C)</th>
<th>Case</th>
<th>Ramp Rate at Switch Point ($/sec)</th>
<th>Final Energy in Molten Fuel (Mw-sec)</th>
<th>Mass of Molten Fuel at Termination (Kg)</th>
<th>Final Average Core Temperature (°K)</th>
</tr>
</thead>
<tbody>
<tr>
<td>3500</td>
<td>Loss-of-Flow (SAS2A ρ/sec)</td>
<td>86.00^a</td>
<td>3.19x10^4</td>
<td>50,600</td>
<td>3750</td>
</tr>
<tr>
<td></td>
<td>Loss-of-Flow (Max ρ/sec)</td>
<td>256.00</td>
<td>4.75x10^4</td>
<td>54,000</td>
<td>4400</td>
</tr>
<tr>
<td></td>
<td>Overpower Transient ($1/sec)</td>
<td></td>
<td>Not Calculated</td>
<td></td>
<td></td>
</tr>
<tr>
<td></td>
<td>Overpower Transient ($10/sec)</td>
<td>9.00</td>
<td>2.17x10^4</td>
<td>47,500</td>
<td>3200</td>
</tr>
<tr>
<td></td>
<td>Overpower Transient ($100/sec)</td>
<td></td>
<td>Not Calculated</td>
<td></td>
<td></td>
</tr>
<tr>
<td>5000</td>
<td>Loss-of-Flow (SAS2A ρ/sec)</td>
<td>139.00</td>
<td>5.36x10^4</td>
<td>54,000</td>
<td>5150</td>
</tr>
<tr>
<td></td>
<td>Loss-of-Flow (Max ρ/sec)</td>
<td>256.00</td>
<td>5.61x10^4</td>
<td>55,400</td>
<td>5200</td>
</tr>
<tr>
<td></td>
<td>Overpower Transient ($1/sec)</td>
<td>125.00</td>
<td>5.00x10^4</td>
<td>53,500</td>
<td>5200</td>
</tr>
<tr>
<td></td>
<td>Overpower Transient ($10/sec)</td>
<td>29.00</td>
<td>3.91x10^4</td>
<td>52,500</td>
<td>4900</td>
</tr>
<tr>
<td></td>
<td>Overpower Transient ($100/sec)</td>
<td></td>
<td>Not Calculated</td>
<td></td>
<td></td>
</tr>
<tr>
<td>6000</td>
<td>Loss-of-Flow (Max ρ/sec)</td>
<td>256.00</td>
<td>7.08x10^4</td>
<td>55,700</td>
<td>6150</td>
</tr>
<tr>
<td></td>
<td>Overpower Transient ($1/sec)</td>
<td>138.00</td>
<td>5.55x10^4</td>
<td>54,300</td>
<td>5500</td>
</tr>
<tr>
<td></td>
<td>Overpower Transient ($10/sec)</td>
<td>100.00</td>
<td>5.34x10^4</td>
<td>54,100</td>
<td>5450</td>
</tr>
<tr>
<td></td>
<td>Overpower Transient ($100/sec)</td>
<td></td>
<td>81.00</td>
<td>4.69x10^4</td>
<td>53,600</td>
</tr>
</tbody>
</table>

*Total Fuel Mass (excluding radial blanket) = 82,600 Kg

^aSAS2A sign error (should be negative ρ/sec)
NOTE: EACH TEMPERATURE REPRESENTS THE RADIAILY AVERAGED, HOTTEST AXIAL FUEL NODE TEMPERATURE.

FIGURE 1. 100°C SUPERHEAT, LOSS-OF-FLOW PREDISASSEMBLY POWER HISTORIES

FIGURE 2. 1000 Mwe SODIUM VOIDING PROFILE (100°C SUPERHEAT LOSS-OF-FLOW) -- 3500°C SWITCH POINT

FIGURE 3. FOUR ZONE, 4000 Mwe SODIUM VOIDING PROFILE (100°C SUPERHEAT, LOSS-OF-FLOW) -- 3500°C SWITCH POINT

FIGURE 4. FOUR ZONE, 4000 Mwe SODIUM VOIDING PROFILE (10°C SUPERHEAT, LOSS-OF-FLOW) -- 3500°C SWITCH POINT
FIGURE 5. $1/\text{sec}$ OVERPOWER TRANSIENT PREDISASSEMBLY POWER HISTORIES (100°C SUPERHEAT)

FIGURE 6. $10/\text{sec}$ OVERPOWER TRANSIENT PREDISASSEMBLY POWER HISTORIES (100°C SUPERHEAT)

FIGURE 7. $100/\text{sec}$ OVERPOWER TRANSIENT PREDISASSEMBLY POWER HISTORIES (100°C SUPERHEAT)

FIGURE 8. $1/\text{sec}$ OVERPOWER TRANSIENT PREDISASSEMBLY POWER HISTORIES (10°C SUPERHEAT)
energy deposited in the molten fuel was somewhat increased by radial power flattening. For example, the increase in energy deposited in the molten fuel for the four zone, 4000 Mwe core over the two zone, 4000 Mwe core ranged from approximately 4%, for the 3500°C switch, 100°C superheat, loss-of-flow accident, to 15% for the 6000°C switch, 100°C superheat, loss-of-flow accident. The yields and reactivity insertion rates at the switch points were not very sensitive to the assumed superheat.

One of the most important conclusions which can be drawn from the results of the safety studies presented in this paper concerns the effects of the multiple disassembly switch points on the calculations. In some cases, reactivity oscillations were such that the high temperature switch points did not correspond to a super-prompt-critical configuration. The difference in timing between reactivity changes resulting from sodium voiding and Doppler broadening is the main reason that the above behavior occurred. In other words, cores which begin voiding early in the calculation may have negative ramp rates at high temperatures compared with cores that begin voiding relatively later. The converse is also true. These uncertainties in the predisassembly-disassembly switch point indicate a possible area of important further research. Since the calculations and energy releases are sensitive to where and when disassembly calculations begin, future research might emphasize the development of models which explicitly calculate the predisassembly-disassembly switch point.

IV. REFERENCES


ON THE EQUATION OF STATE OF MIXED OXIDE FUEL FOR THE
ANALYSIS OF FAST REACTOR DISASSEMBLY ACCIDENTS

H.G. Bogensberger, E.A. Fischer and P. Schmuck

Institut für Angewandte Systemtechnik und Reaktorphysik
Institut für Neutronenphysik und Reaktortechnik
Kernforschungszentrum Karlsruhe, West Germany

Abstract: In this work the vapor pressure for mixed oxide is calculated by following the method used by Rand and Markin. The vapor pressure of the fission products is also calculated, and found to be much larger than that of mixed oxide at temperatures around the melting point. The disassembly phase of an unprotected reactivity accident in a LMFBR is analyzed using these vapor pressure data. The accident is initiated by a $5 \$/sec reactivity ramp; the predisassembly part was analyzed earlier with the SAS-2A code. It is found that the vapor pressure of the fission products has the effect of greatly reducing the energy release in the accident.

1. Introduction

In the LMFBR safety analysis, the estimation and discussion of the energy released in a core disassembly accident plays an important role. Therefore, a major effort is devoted to the study of such prompt critical accidents in several laboratories. The activities at Karlsruhe in this area include the development of codes and models, experimental verification of the models, and work to obtain the necessary thermodynamic data. Recently, the codes CAPRI-2 and KADIS became available to analyze the predisassembly, and the disassembly phase.

When one studies a disassembly accident, one starts with the highly hypothetical assumption that the shutdown system fails completely when either a reactivity ramp, or a pump failure occurs. Then, sodium voiding or fuel slumping cause a fast reactivity ramp, which inspite of some compensation by the Doppler effect, leads to a prompt critical nuclear excursion. The evaporation of fuel and the outward movement of core material due to the associated pressure gradient, represents the shutdown mechanism which ultimately terminates such an excursion. In the studies published so far /1/, the fuel was treated as pure UO$_2$, and it had to be heated well above the boiling point until the pressure required for a disassembly was reached. Therefore, the analysis predicted a substantial energy release in such an excursion, and a rather large amount of molten fuel present in the core after the shutdown.
It is to be expected that the onset of disassembly, and therefore the energy produced until shutdown, depends largely on the equation of state data used for the fuel material. The data used so far were essentially those obtained by Menzies /2/ for UO$_2$ in 1966.

Menzies made extensive use of the principle of corresponding states to obtain the equation of state for UO$_2$ from the experimental values available. However, the behavior of mixed oxide fuel may be different from that of pure UO$_2$; furthermore, the fuel contains, after some burn up, a sizable amount of fission products, and many of them are more volatile than the fuel material. Therefore, use of the UO$_2$ data in the analysis of a disassembly accident is rather conservative, and an attempt is made in this paper to evaluate and to use data for actual oxide fuel, including the effect of fission products. However the multicomponent system of fuel and fission products is rather complicated. The thermodynamic data are known only at low temperatures, and have to be extrapolated. Also, axial and radial migration phenomena are not known accurately. Therefore, the present data are to be considered as a first approximation, and further work is needed in this area. However, it is believed that the order of magnitude is correct, and it can be assumed that the energy release, obtained with realistic data, is certainly much lower than the one obtained with UO$_2$ data.

2. Evaluation of the vapor pressure data of mixed oxide

The evaluation of the vapor pressure of mixed oxide follows the method used by Rand and Markin /3/. It is assumed that in the substoichiometric crystal, UO$_2$ and PuO$_{2-z}$ go into solution ideally, and in the hyperstoichiometric case UO$_{2+z}$ and PuO$_2$ go into solution ideally. The oxygen potential is determined from experimental data.

More sophisticated models are also available, which may be used to obtain the oxygen potential of mixed oxide. For example, Thorn and Winslow /4/ used the statistical theory of crystal defects to describe the stoichiometry dependence of the thermodynamic functions. More recently, Blackburn /5/ proposed a semi-empirical model based on the law of mass action, and obtained the thermodynamic data from the free energies of formation of oxides, and from phase boundary data. However, the major problem is that one cannot apply these models to the liquid phase with confidence. Therefore, a straight extrapolation of the Rand and Markin data is used, until more detailed information becomes available.

Method of calculation

The reaction equations for a mixed oxide of the form U$_{1-x}$Pu$_x$O$_{2-z}$ are as follows:

$$\text{PuO}_{2-z}(s) + \frac{z}{2}O_2 \rightarrow \text{PuO}_2(g) \quad (1)$$

$$\text{UO}_2(s) \rightarrow \text{UO}_2(g) \quad (2)$$

$$\text{PuO}(g) + \frac{1}{2}O_2 \rightarrow \text{PuO}_2(g) \quad (3)$$

$$\text{UO}_2(g) + \frac{1}{2}O_2 \rightarrow \text{UO}_3(g) \quad (4)$$

Where (s) refers to the solid state, (g) to gas.

Similar equations hold for the hyperstoichiometric case.
The equation of state for mixed oxide was obtained by extrapolating Rand and Markin's data up to 5000 K.

For the reaction (1), the change in free energy is

\[ \Delta G = \Delta G_{\text{PuO}_2}^{(g)} - \Delta G_{\text{PuO}_{(2-z)}}^{(s)} - RT \ln x \]  

(5)

where the last term represents the free energy of solution in the crystal.

Furthermore

\[ \Delta G = -RT \ln K_p \]  

(6)

with

\[ K_p = \frac{P(\text{Pu}_0 \gamma_1 \cdot P(\text{O}_2)^{\gamma_2}}{2} \]  

(7)

By combination of the equations (5) and (7), one obtains

\[ RT \ln \left[ P(\text{Pu}_0 \gamma_1 \cdot P(\text{O}_2)^{\gamma_2} \right] - \frac{z}{2} \]  

(8)

This relation, and similar ones for the reactions (2) to (4) determine the partial pressures of all gaseous species as a function of the oxygen potential. These equations were used to extrapolate the vapor pressure to the high temperatures which occur in a disassembly accident.

3. Vapor pressure of the fission products

In order to evaluate the vapor pressure of fission products in a medium with a given oxygen potential, like mixed oxide, one has to take into account the oxidation states of the different materials. The reaction equations for oxidation, and for evaporation can most conveniently be written by considering the formation of gaseous oxides from gaseous metal

\[ aM(g) + \frac{b}{2} O_2 \rightarrow M_a O_b (g) \]  

(9)

and of condensed compounds from gaseous metal

\[ aM(g) + \frac{b}{2} O_2 \rightarrow M_a O_b (c) \]  

(10)

The case \( b = 0 \) in equation (10) takes into account the condensation of pure metal.

The equilibrium constant for reaction (9) is given by

\[ K_p = \frac{(pM)^a}{(pO_2)^{b/2}} \]  

(11)

For reaction (10) the denominator is replaced by the activity \( a(M_a O_b) \).

The constant \( K_p \) is connected with the change in free energy.

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\[ \Delta G = a \Delta G(M) - \Delta G(M_{\text{ab}}). \]

Considering that from the ideal gas law the vapor pressure is given by

\[ p = n \frac{RT}{v} \]

where \( n \) is the number of moles per cm\(^3\) and \( v \) is the "free" volume of the mixed oxide.

For the condensed materials, we assume that the activity is given by the molar fraction of the component under consideration.

\[ a = \frac{n}{\Sigma n} \]

where \( \Sigma n \) is to be extended over all the materials in which the component can be considered to be soluble. It is not easy to decide in which materials a given compound is soluble. For a first evaluation, the sum is extended over metals for a metal, and over all the oxides present for an oxide. With these assumptions, one obtains the equilibrium concentration and the pressures from the relations (11), and from the fission yields. The calculation is carried out in a computer program VAPRES. Note that the pressures for the calculations reported in section 6 were obtained with a simplified model.

**Fission gas distribution**

The spatial distribution of the fission gas in a pin depends on the amount of gas retained in the lattice, and the amount released into intergranular space. A large amount of effort has been involved during the last years to treat this problem. Our assumption is based on the work of Ronchi and Matzke /6/. For the balance of fission gas one obtains

\[ \beta \cdot t = c + b + g \]

\( \beta \) production rate of fission gas [moles/cm\(^3\) sec]

\( c \) concentration of dissolved gas

\( b \) concentration of gas precipitated into intragranular bubbles

\( g \) concentration of gas precipitated in grain boundaries and eventually released into intergranular space

\( t \) operating time, sec

As a first step one needs a correlation of the distribution of gas filled bubbles with the concentration of gas atoms in solution. For this purpose we assume a homogeneous distribution of the bubbles.

Inside of a volume element \( 6V \), which is the Wigner-Seitz cell associated with one bubble, one has the diffusion equation

\[ \dot{c} = D \Delta c \]

During the diffusion process, the spatial equilibrium is approached quickly, and the time dependence of the equilibrium distribution is given by

\[ c = c_0 \exp \left( -t/\tau \right) \]

with \( \tau = (4\pi n r_0 D)^{-1} \),

\[ 1336 \]
where \( n \) is the concentration of bubbles per \( \text{cm}^3 \), \( D \) the diffusion coefficient, and \( r_o \) the radius of the bubble.

The derivative is

\[
\frac{\partial}{\partial t} c = -\frac{c}{T}
\]

Equation (13) gives the concentration of gas in solid material for a uniform distribution of bubbles. But under operational conditions the case is more complicated. One has to take into account:

- gas is produced continuously with the rate \( \beta \)
- fission events cause a certain resolution
- a certain amount of gas precipitates at grain boundaries where it may behave differently from gas in an intragranular bubble.

The resolution rate of gas can be calculated with the model of Nelson /7/

\[
\Gamma = \frac{4\pi r_o^2 d \cdot n}{b}
\]

\( \Gamma \) resolution rate for the gas in one bubble, mole/sec
\( b \) Van der Waals covolume, \( \text{cm}^3/\text{mole} \)
\( n \) escape probability, sec^{-1}
\( d \) shell thickness of bubble, cm

Under the assumption that growth of bubble does not create strains around the surface, the concentration (in moles) of gas in a bubble is given by

\[
m = \frac{8\pi \sigma r_o^2}{3 \cdot RT}
\]

\( \sigma \) surface tension

with a concentration \( n \) of bubbles per \( \text{cm}^3 \) and homogeneous growth one obtains

\[
\frac{\beta t - c^X}{n} = \frac{8\pi \sigma r_o^2}{3 \cdot RT}
\]

\( c^X \) not precipitated in bubbles

By introduction of (15) into (14) the resolution rate is given by

\[
\Gamma = (\beta t - c^X) \frac{3}{2} \frac{RTd \cdot n}{b\sigma}
\]

Furthermore we need a treatment of the precipitation of gas at grain boundaries. If one assumes that atomic diffusion of the gas is the only transport mechanism, one obtains the following relation for the flux of gas toward grain boundaries /8/

\[
g = c \left[ 1 - \sum_{n=1}^{\infty} \frac{6}{\pi^2 n^2} \exp \left( -\frac{n^2 \pi^2 D t}{a^2} \right) \right]
\]

where \( a \) is the radius of the grain, and \( D \) the diffusion coefficient.

For the derivative follows
\[ g' = \text{Pos} \left( \frac{dc}{dt} \right) \left[ 1 - \sum_{n=1}^{\infty} \frac{6}{n^2 \pi^2} \exp \left( -n^2 \pi^2 \frac{Dt}{a^2} \right) \right] \]
\[ + \frac{6cD}{a^2} \left( \sum_{n=1}^{\infty} \exp \left( -n^2 \pi^2 \frac{Dt}{a^2} \right) \right) \]

By using (13), (16) and (17a) one obtains the final expression /5/

\[ \frac{dc}{dt} = \beta - \frac{Kc}{\sqrt{\beta}} (\beta t - c - g) + C_o (\beta t - c - g) - \frac{dg}{dt} \]

\[ \frac{dg}{dt} = \text{Pos} \left( \frac{dc}{dt} \right) \left[ 1 - \sum_{n=1}^{\infty} \frac{6}{n^2 \pi^2} \exp \left( -n^2 \pi^2 \frac{Dt}{a^2} \right) \right] + \]

\[ + \frac{6cD}{a^2} \left( \sum_{n=1}^{\infty} \exp \left( -n^2 \pi^2 \frac{Dt}{a^2} \right) \right) \]

\[ K = D \left( \frac{6\pi n^2 RT}{\sigma} \right)^{1/2} \]

\[ C_o = \frac{3}{2} \frac{R \cdot T \cdot d \cdot n}{b \cdot \sigma} \]

Equation (18) is evaluated in a subroutine for various operating temperatures and reactor operating times. However, one has to consider that the portion \( g/\beta t \) of the gas, which is released into intergranular space, is free to diffuse into the free space in the core section, blanket section, and fission gas plenum. It is assumed that the gas is in pressure equilibrium in the different sections of the pin. Thus, the fraction left in the core section is

\[ \alpha = \frac{V_{c/T}}{V_c} - \frac{V_{b1}}{V_{b1}} - \frac{V_{pl}}{V_{pl}} \]

where \( V = \) empty volume, \( T = \) temperature. The sections of the pin are \( c = \) core, \( b1 = \) blanket, \( pl = \) fission gas plenum. The fraction \( \alpha \) is evaluated using the temperatures at normal operation. It is assumed that no transfer of gas into the plenum occurs during an accident. Typically, \( \alpha \) is of the order of 7 %. If there is a hypothetical accident postulated the data obtained in this way were used as the initial concentrations for an overpower excursion.

**Data**

The concentration of the different fission products is influenced by the production, but also by radioactive decay. Under continuous reactor operation an equilibrium will be established. Yields for the equilibrium situation were evaluated in /9/. In this evaluation, the effect of \( (n, \gamma) \)-reactions of the fission products are also taken into account for a flux of \( 10^{14} \) n/cm\(^2\)-sec. The fission yields used in this paper, corresponding to a fission spectrum
of the incident neutrons are shown, for $^{239}\text{Pu}$ and $^{238}\text{U}$, in Table 1. The thermodynamical data, e.g. the free energy of formation, were used as published in /10/, /11/.

5. Calculated vapor pressure

The vapor pressure of mixed oxide, as calculated from the equations in Section 2, is shown in Fig. 1 for two different values of the stoichiometry. The ratio $x = \text{Pu}/(\text{Pu} + \text{U})$ is 0.15. Also shown is the pressure for pure $\text{UO}_2$, as taken from the ANL-equation of state published in /12/. At temperatures below about 2500 K, the pressure changes by more than an order of magnitude as a function of the stoichiometry, although it is very small in magnitude. Above about 4000 K, where the pressure becomes significant, the dependence on the stoichiometry is rather weak.

The vapor pressure of the fission products was calculated with the program VAPRES, using the equations in Section 3. Results for a burn up of 3%, and smear density 5 g/cm$^3$, are listed in the following table. They may be compared to a similar evaluation published by Gabelnick and Chasanov /13/.

<table>
<thead>
<tr>
<th>T, K</th>
<th>3000</th>
<th>4000</th>
<th>5000</th>
</tr>
</thead>
<tbody>
<tr>
<td>total pressure</td>
<td>101</td>
<td>196</td>
<td>694</td>
</tr>
<tr>
<td>$\text{Kr} + \text{Xe}$</td>
<td>65</td>
<td>108</td>
<td>150</td>
</tr>
<tr>
<td>other fission products</td>
<td>36</td>
<td>83.5</td>
<td>346</td>
</tr>
<tr>
<td>mixed oxide</td>
<td>0.014</td>
<td>4.5</td>
<td>198</td>
</tr>
<tr>
<td>oxygen</td>
<td>$1.6 \times 10^{-6}$</td>
<td>0.0004</td>
<td>0.0091</td>
</tr>
</tbody>
</table>

The fission products which contribute significantly to the vapor pressure are $\text{Cd}$, $\text{Cs}$, $\text{J}$, $\text{Kr}$, $\text{Rb}$, $\text{Te}$, $\text{Xe}$. A comparison with Ref. /13/ shows that the data are generally consistent, though the partial pressure of oxygen is rather different. However, the pressure of mixed oxide at 5000 K is much higher in the present evaluation.

In a reactivity accident, to be discussed in Section 6, the core is filled with sodium, and the only space available for the vapor is given by the smear density which is of the order of 85% of the theoretical density. Vapor pressures which are typical for this case are shown in Fig. 2, as a function of temperature. The step at the melting temperature of 3070 K occurs because the molten fuel occupies more space than the solid one.

6. Analysis on an unprotected reactivity accident using the evaluated vapor pressure data

The Karlsruhe disassembly code KADIS was used to study the implications of the evaluated data on the analysis of a reactivity accident. The code KADIS calculates the feedback from the Bethe-Tait mechanism using compressible hydrodynamics, and from the Doppler effect. It can handle two dimensions, and several enrichment zones, and is about equivalent to the ANL code VEIJUS-2 /14/.

The analysis was performed for the 300 MWe LMFBR prototype SNR-300, Mark I core. The core has two enrichment zones, the core cross section is shown in Fig. 3. The volume fractions for fuel/Na/SS are 32/48/20. The accident under study is initiated by an unprotected 5%/sec reactivity ramp. The predisassembly part was analyzed with the code SAS-2A at ANL; the analysis is
published in Ref /1/, which also contains a more detailed description of the reactor. At the point in time where the SAS calculation ends, an $1^8$ $\$/sec ramp is going on, caused by fuel coolant interaction and expulsion of sodium. However in order to demonstrate clearly the implications of the vapor pressure data, a ramp of 50 $\$/sec was instead assumed in KADIS. This assumption is rather conservative. However, release of fission gas after pin failure may speed up the expulsion of sodium, and thus cause a higher ramp rate than calculated. All the other input data were taken from the SAS calculation. Note that in this case, the SAS run was terminated before the cell-averaged fuel temperature reached the melting point. Thus the melting process, and build up of fission product vapor pressure, is described in the KADIS calculation. Under these circumstances, the energy release calculated for the disassembly phase is quite substantial, because of the early starting point. Some of the important input data are listed below. The radial power distribution is shown in Fig. 4.

**Input data for KADIS**

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Power</td>
<td>76 540 MW</td>
</tr>
<tr>
<td>Maximum temperature</td>
<td>3005 K</td>
</tr>
<tr>
<td>Assumed melting temperature</td>
<td>3040 K</td>
</tr>
<tr>
<td>Average smear density of fuel</td>
<td></td>
</tr>
<tr>
<td>core zone 1</td>
<td>9.36 $\text{g/cm}^3$</td>
</tr>
<tr>
<td>core zone 2</td>
<td>9.55 $\text{g/cm}^3$</td>
</tr>
<tr>
<td>Reactivity ramp</td>
<td>50 $$/sec</td>
</tr>
<tr>
<td>Initial reactivity</td>
<td>1.006 $$</td>
</tr>
<tr>
<td>$\beta_{eff}$</td>
<td>0.00304</td>
</tr>
<tr>
<td>Fission gas fraction $a$ in the core</td>
<td>0.066</td>
</tr>
<tr>
<td>(central channel)</td>
<td></td>
</tr>
</tbody>
</table>

The analysis shows that the energy released during disassembly is largely reduced if fission product vapor pressure is involved. However, the behavior of the fission gas depends on the sequence of events during the accident, and a more careful analysis of the predisassembly phase would be needed in order to decide how much fission gas is available during disassembly. Therefore, it was decided to study separately the effect of the noble gases, and of the non-gaseous fission products. Furthermore, the vapor pressure data for the non-gaseous fission products obtained in this paper should be considered as estimates, because effects like migration of fission products, formation of compounds between two or more fission products (e.g. CsJ), and deviations from thermodynamic equilibrium during the rapid heating in the accident were not taken into account. It is probably very difficult to assess all these effects quantitatively.

Thus, it may not be justified to assume that the calculated vapor pressure of the fission products is fully effective. Therefore, we have also studied cases under rather conservative assumptions where the magnitude of the pressure is reduced, and where the criterion for the build up of the pressure is changed.

The following assumptions were made for the different cases. Note that in the cases 1 to 4 no noble gas pressure is included; case Nr. 5 includes the full pressure of the gases.
Case Nr.
1 vapor pressure calculated as described in Sections 2 and 3. The pressure becomes effective as the fuel melts, i.e. it is proportional to the melt fraction in the mesh cell.
2 the vapor pressure of case 1 is reduced by 50%.
3 magnitude of vapor pressure calculated as described. However only 20% become effective as the fuel melts. The other 80% are released only when the melt fraction exceeds 90%. In this case, it is assumed rather conservatively that part of the fission products have migrated radially towards the edge of the pellet, and are released only when melting progresses towards the outer region of the pellet.
4 the vapor pressure of case 3 is reduced by 50%.
5 the pressure of the noble gases is included. The pressure of the released gas is effective before the fuel melts. The retained gas is released in proportion to the melt fraction. In addition, the pressure of case Nr. 2 is included.

For comparison, one case was calculated with the ANL equation of state /12/. This equation is based on the Menzies data for UO₂ /2/, and was used in the work reported in Ref. /1/.

7. Results and discussion

The results of our calculations are gathered in Tab. 2 to 5. The power history for some of the cases is shown in Fig. 5. Even if fission gases are ignored, the energy release is much lower than with the ANL data. It increases from Case No. 1 to No. 4, with No. 4 being rather pessimistic, and No. 1 probably too optimistic. The total energy release during disassembly comes out to be rather high in most of the cases, because the temperatures at the beginning of the KADIS calculations are fairly low. Therefore the mass of the molten fuel (Table 4) present at the end of disassembly, or the energy contained in the molten fuel are more suitable parameters to characterise the damage potential of the excursion. If these parameters are considered, the vapor pressure of the fission products causes an even more drastic reduction in comparison to the results obtained with the ANL-data. The tables also illustrate the difference between the case where the pressure is built up at the beginning of melting (cases 1 and 2), and those where the pressure release is practically delayed until 80% of the fuel is molten (cases 3 and 4). This difference is clearly recognized by looking at Table 5, which shows the maximum fuel temperature, or the maximum melt fraction in the mesh cell. At 3% burn up, the melting temperature is exceeded only in the cases 3 and 4.

If the fission gas pressure is included (case 5), the disassembly occurs even earlier (compare Fig. 5). In the case of 1% burn up, the maximum melt fraction in the hottest cell is only 16%, and the mass of molten fuel is very small indeed. This result is based on the assumption that the fission gas present in the core under operating conditions is fully available for the pressure build up. The validity of this assumption can only be checked by a more careful analysis of the predisassembly part of the accident. This fact does not touch the general conclusion that the disassembly energy in an irradiated core is determined by the fission product pressure, and is remarkably lower than in a fresh core, which contains pure UO₂. This has been demonstrated for the reactivity ramp accident, when the core is filled with sodium, and the free volume is very small; in the case of pure UO₂, high single-phase pressures cause disassembly.

Similar results are to be expected for sodium-out cores. Though the fission
product pressure is lower because of the larger free volume, it competes only with the saturation pressure in the case of pure UO₂, which is much lower than the single-phase pressure.

Table 1  Fission Yields for Pu²³⁹ and U²³⁸ (for 100 fissions)

<table>
<thead>
<tr>
<th>Pu²³⁹</th>
<th>U²³⁸</th>
</tr>
</thead>
<tbody>
<tr>
<td>Ge 0.01</td>
<td>Ce 0.01</td>
</tr>
<tr>
<td>As 0.01</td>
<td>Se 0.01</td>
</tr>
<tr>
<td>Se 0.13</td>
<td>Br 0.12</td>
</tr>
<tr>
<td>Br 0.11</td>
<td>Kr 3.0</td>
</tr>
<tr>
<td>Kr 1.80</td>
<td>Rb 2.22</td>
</tr>
<tr>
<td>Rb 1.33</td>
<td>Sr 8.6</td>
</tr>
<tr>
<td>Sr 5.30</td>
<td>Y 4.0</td>
</tr>
<tr>
<td>Y 2.55</td>
<td>Zr 26.83</td>
</tr>
<tr>
<td>Zr 21.6</td>
<td>Nb 0.03</td>
</tr>
<tr>
<td>Mo 24.70</td>
<td>Mo 24.68</td>
</tr>
<tr>
<td>Ru 27.80</td>
<td>Ru 23.60</td>
</tr>
<tr>
<td>Pd 6.75</td>
<td>Ph 3.20</td>
</tr>
<tr>
<td>Ag 1.75</td>
<td>Pd 3.52</td>
</tr>
<tr>
<td>Cd 0.29</td>
<td>Ag 0.40</td>
</tr>
<tr>
<td>Cd 0.05</td>
<td>Cd 0.17</td>
</tr>
</tbody>
</table>

Table 2  TOTAL ENERGY RELEASE DURING THE DISASSEMBLY PHASE, (MJ)

<table>
<thead>
<tr>
<th>AVERAGE BURN UP (%)</th>
<th>CASE NO.</th>
<th>ANL-EQU. OF STATE</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>1 2 3 4 5</td>
<td></td>
</tr>
<tr>
<td>1</td>
<td>873 1120</td>
<td>1710 197</td>
</tr>
<tr>
<td>3</td>
<td>608 760</td>
<td>1030 1280 100</td>
</tr>
<tr>
<td>5</td>
<td>521 872</td>
<td></td>
</tr>
<tr>
<td>8</td>
<td>457 745</td>
<td>3270</td>
</tr>
</tbody>
</table>
### Table 3: Energy in the Molten Fuel After Disassembly (MJ)

<table>
<thead>
<tr>
<th>Average Burn Up (%)</th>
<th>Case No.</th>
<th>ANL-EQU. Of State</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>1</td>
<td>2</td>
</tr>
<tr>
<td>1</td>
<td>116</td>
<td>200</td>
</tr>
<tr>
<td>3</td>
<td>49</td>
<td>84</td>
</tr>
<tr>
<td>5</td>
<td>36</td>
<td></td>
</tr>
<tr>
<td>8</td>
<td>27</td>
<td></td>
</tr>
</tbody>
</table>

### Table 4: Mass of Molten Fuel After Disassembly, (KG)

<table>
<thead>
<tr>
<th>Average Burn Up (%)</th>
<th>Case No.</th>
<th>ANL-EQU. Of State</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>1</td>
<td>2</td>
</tr>
<tr>
<td>1</td>
<td>412</td>
<td>688</td>
</tr>
<tr>
<td>3</td>
<td>174</td>
<td>298</td>
</tr>
<tr>
<td>5</td>
<td>127</td>
<td></td>
</tr>
<tr>
<td>8</td>
<td>97</td>
<td></td>
</tr>
</tbody>
</table>

### Table 5: Maximum Fuel Temperature (K) or Maximum Melt Fraction in the Cell (%) After Disassembly

<table>
<thead>
<tr>
<th>Average Burn Up (%)</th>
<th>Case No.</th>
<th>ANL-EQU. Of State</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>1</td>
<td>2</td>
</tr>
<tr>
<td>1</td>
<td>3044 K</td>
<td>3199 K</td>
</tr>
<tr>
<td>3</td>
<td>67 %</td>
<td>86 %</td>
</tr>
<tr>
<td>5</td>
<td>56 %</td>
<td></td>
</tr>
<tr>
<td>8</td>
<td>48 %</td>
<td></td>
</tr>
</tbody>
</table>
References


/5/ P.E. Blackburn, J. Nucl. Mat. 46, 244 (1973)


/11/ Gmelin: Handbuch der Chemie

/12/ W.T. Sha, T.H. Hughes, ANL-7701 (1970)

/13/ S.D. Cabelnick, M.G. Chasanov, ANL-7867 (1972)

/14/ J.F. Jackson, P.B. Nicholson, ANL-7951 (1972)
Fig. 1  Vapor pressure of fuel as a function of the temperature

Fig. 2  Fission product vapor pressure (smear density 8.5 g/cm³)

Fig. 3  Map of the SNR-300 Mark 1 core
Fig. 4  Radial power distribution

Fig. 5  Total reactor power
SESSION 13
ALTERNATE SHUTDOWN SYSTEMS
AND OTHER CONSIDERATIONS
Chairman: M. Levenson (EPRI)
"Diverse Shutdown Systems for the KNK-1, KNK-2 and SNR-300 Reactors"
F.H. Morgenstern, H. Buchholz, H. Krüger, H. Röhrs
INTERATOM, Bensberg, W.-Germany

Abstract:

The situation of KNK-1, in which a second fast acting shutdown system was strongly required by the authorities, was very unfavorable due to the advanced stage of construction. The available time could be utilized only to develop and install a mechanical system. Herewith a second shutdown system was predetermined for KNK-2, which has been modified within the mechanical part only a little. The availability of a functioning system and the open-ended development expenses, obvious from studies of a liquid absorber system, resulted in the decision to consider the already developed system as a basic design for the SNR-300, too. This decision was supported by the fact that it was impossible to define specific criteria for the general demand for diversity. The degree of diversity and independence achieved in the whole safety system including the shutdown systems, however, seems to be sufficient with respect to the very remote probability of a subassembly event intolerably damaging the absorber guide tubes of both shutdown systems, sufficient in the sense of an overall reliability.
1. Introduction

Starting from the specific requirements for the diverse (i.e. second) shutdown systems of the German reactors KNK-1, KNK-2 and SNR-300, which have been either proposed by the designer or imposed by the licensing authorities, it will be shown, how technical solutions have been found. Thereby one has to take into account that the requirements had not been fixed at the very beginning of the design work, but have been defined iteratively during the licensing process. The following presentation will deal not only with the shutdown rods and drive mechanisms and the related experimental development work, but also with the actuating part of the plant safety system, since the general demands for redundancy and diversity have been increasingly extended to all parts of the safety system.

The main features of 3 reactors will be reported, although the KNK-2 is, of course, not a new reactor plant when compared with KNK-1. With respect to the specific requirements for the plant safety system, however, which have been changed considerably due to the modified type of core, it is, nevertheless, a new reactor within the scope of this work.

The reasons for including all three reactors, although KNK-1 is a thermal system, are the immediate transfer of know-how from the KNK-1 to the other two reactor systems, the clear recognition of the historical development of techniques and the escalation of requirements. Therefore, the requirements will be specified and the principles of the chosen solution illustrated. Especially the distinguishing features between the first and the second shutdown system will be identified.

To complete the illustration of the design work the experience gathered up to now and the experimental programs will be reported.

Some remarks will finally be given as to an alternative considered during the development and design work, which has not been selected as a practicable solution.
2. Requirements and technical solutions

2.1 KNK-1

Requirements: At the end of 1966 the responsible licensing authorities required a second fast acting shutdown system for KNK-1. By that time design of the plant was practically completed, construction had been underway since May and essential components were already in the manufacture, among others the reactor vessel, the plug and the core components. The requirement resulted from a general feeling of uncertainty about a rather new type of reactor, the shutdown capability of which needs a back-up against unknown and unexpected core deformation or misalignment due to the given temperature gradients. These and other unidentified sources may cause a common mode failure to a single fast acting shutdown system. Based on these assumptions four essential mechanical design criteria have been specified (see table 1) for the second shutdown system of KNK-1.

Since the reactor has a positive coolant and moderator temperature coefficient of reactivity all disturbances leading to an increase in temperature tend to increase reactivity. This effect must play a role in determining the necessary shutdown reactivity and its rate of change. The accidents to be considered include the sudden pipe rupture, pump seizure and blockage or valve closing, but all of which can be detected by the signal "rate of change of the sum of coolant flow in both loops". This signal trips the reactor even in the extreme situation of a sudden pipe rupture early enough, to avoid local boiling (i.e. about 1.5 sec after the accident). Even in this case the nuclear controllability by one shutdown system could be demonstrated.

The rate of change of the coolant flow during the other accidents is rather slow. This fact gave the argument for the trip criterion of the second shutdown system: 3 seconds after a pump blockage, the most serious accident, the average core outlet temperature of the coolant reaches the boiling point, therefore shutdown of the reactor by both systems has to be completed within this time period.
Table 1: Requirements for the second shutdown system of KKN-1

1. The design principle should be different to the highest possible degree when compared with the 1st system

2. It should be capable of handling deformation and misalignment which could impair shutdown function by the 1st system

3. It should be completely independent from the 1st system

4. It has to be incorporated into the rather advanced status of the design of the respective components

5. Shutdown should be completed within 3 seconds

6. Shutdown reactivity and its rate of change should be oriented on change of reactivity related to coolant flow and reactivity accidents to be considered

7. A sufficient degree of subcriticality incl. all calculational uncertainties should be guaranteed

8. No reduction of plant availability should arise from the mere existence or operation of this second shutdown system

A further requirement results from the considered reactivity accidents, the most serious of which is the simultaneous runout of all control-rods of the second shutdown system with the maximum possible speed of their drives.

If, furthermore, a safe subcriticality has to be guaranteed during shutdown condition of the reactor, the criteria 5 to 7 of table 1 can be formulated.

In the course of the development work which will be sketched later on, a further criterion has turned out to be very important, but not so much from a safety point of view, namely the plant availability criterion, item No. 8 of table 1.
Design: Since the basic design of a second shutdown system for all 3 reactors has been developed for KNK-1 the main development steps in the direction of an acceptable solution shall be briefly outlined. In consideration of the advanced stage of fabrication of the core internals, thoughts about possible technical solutions started with thimbles at 3 selected core positions. They had to be filled with a granular absorber material in order to shut down the reactor. The design principle looked rather simple at the first glance, but the filling mechanism turned out to be highly complicated and the void volume during normal operation would have considerably disturbed neutron economy.

The second design also had thimbles but with a central displacement rod, thus forming an annulus. The absorber material in some form would have filled this annulus in order to shut down the reactor. An appropriate absorber material could not be found. The most important, finally unresolved problems were mainly (1) to find a fluid or granular material which could be forced into this annulus fast enough at the surrounding temperature, (2) to remove this material after shutdown and (3) to incorporate the necessary amount of reactivity worth.

The third design then anticipated an articulated, therefore flexible, chain of solid absorbing material filled in wrapper tubes. The guide tube was again the thimble with the central displacement rod. Shock absorption would have been accomplished by a spike, centrally attached to the absorber and entering loose material at the bottom of the thimble.

This solution solved the problems of the second design but the shock absorbing function posed a new problem impossible to overcome: the loose material sintered. The expenditure for an open-ended development did not seem to be justified at that time. Furthermore there existed a rather considerable disadvantage with all three designs: drive mechanism, thimble and absorber had to be completely removed before starting fuel handling, since KNK-1 has a rotating plug.
The fourth and final design will be described in some more details. Figure 1 compares the first and second shutdown units in a very simplified manner concentrating on the most important features. The second shutdown system consists of 3 units. During reactor operation the absorber chain connected with the connection rod by means of the scram clutch is positioned right above the core. In order to scram the absorber the inductor current of the scram magnet will be interrupted. Then the armature plate together with a special release rod inside the connection rod moves downwards a short distance by the force of the tensioned release spring, here-with stretching the claws of the scram clutch apart and releasing the absorber (B\textsubscript{4}C 90% enriched with \textsuperscript{10}B) which drops down by gravity into the annulus formed by the absorber guide tube and a centrally-located Be\textsubscript{0} filled displacement rod. The absorber consists of 4 links each having the shape of a tube section. At the end of the fall hydraulic shock absorption is accomplished by means of a hollow piston and a Belleville spring. Immediately after the scram release action the drive moves the connection rod downwards either to register the completed action or to push down any absorber which may incidentally not have fallen down fully. During the down movement the scram clutch and the armature plate will come into such a position that after turning on of the inductor current the drive can pull the absorber upwards again.

In order to be able to disconnect the absorbers for fuel handling the drives have to be detached and replaced by lifting jigs. By means of these jigs the connection rods have to be lifted by hand via a spindle. The absorbers will remain in the shutdown position during fuel handling.

The first and second systems are interconnected electrically via a time-lag relay. The first system and this relay will be excited by the same sensors of the plant safety system. If there are no signals from the limit switches of the drive rods within a certain time which would indicate the absorber rods of the first system have fallen about 80% of their way, the time-lag relay will release the second system. Otherwise
the relay will be switched off. The tolerable time-lag has been determined from the maximum expectable drop time of the single absorber rods measured in drop tests during pre-operational testing and recurring inspection periods.

In table 2 the most important distinguishing features of both shutdown systems have been compared in order to get an idea of the degree of diversity.

The reactivity worth provided for accident compensation has been made identical to the first system. The following boundary conditions lead to the overall provision:
- rod bank of the first system
  in the most unfavourable position reactivitywise
- a fresh core heated up isothermally to the sodium boiling point at the moment of scram release
- cooling of the sodium and long term Xenon-decay
- 1 $\Xi$-subcriticality (minimum)

2.2 SNR-300

Requirements: Early in 1970 the PSAR for the SNR-300 was submitted to the authorities. INTERATOM was and still is strongly convinced that the strategy of assuring nuclear safety has to be based on the highest possible degree on accident prevention and not mainly on measures like containments to handle the consequences of accidents. The reason is the experience gained during the licensing process supports this theory that the prediction of the course of an accident is much more reliable than the quantitative description of destructive consequences, simply because the field of important parameters widens considerably. Furthermore, it seems to be reasonable to protect the invested capital as well as possible.

Therefore and because KNK-1 was already being licensed on the basis of two shutdown systems it has been decided to incorporate 2 fast acting shutdown systems of equal capabilities into the SNR-300 design from the very beginning.
Table 2: Comparison Between the 1st and 2nd Shutdown System of KNK-1

<table>
<thead>
<tr>
<th></th>
<th>1st Shutdown System</th>
<th>2nd Shutdown System</th>
</tr>
</thead>
<tbody>
<tr>
<td>accident reactivity compensation</td>
<td>identical</td>
<td>identical, but release indirectly by a time-lag relay interconnecting both systems</td>
</tr>
<tr>
<td>sensing, logic system</td>
<td>rigid rod bundle</td>
<td>flexible, articulated 4-tube-section chain</td>
</tr>
<tr>
<td></td>
<td>guide tube</td>
<td>guide annulus</td>
</tr>
<tr>
<td>absorber</td>
<td>rigid</td>
<td>connection rod and release rod with individual joints</td>
</tr>
<tr>
<td>connection rod</td>
<td>rigid</td>
<td>connection rod and release rod with individual joints</td>
</tr>
<tr>
<td>sealing</td>
<td>bellow</td>
<td>elastic sealing rings</td>
</tr>
<tr>
<td>scram release</td>
<td>ball clutch above the plug</td>
<td>claw clutch under sodium</td>
</tr>
<tr>
<td>dropping parts</td>
<td>absorber + transfer rod</td>
<td>absorber chain only</td>
</tr>
<tr>
<td>centering</td>
<td>no centering</td>
<td>centering of the connection rod relative to the absorber guide tube</td>
</tr>
<tr>
<td>max. tolerable misalignment</td>
<td>(9 mm) (between axes of absorber and plug penetration)</td>
<td>20 mm</td>
</tr>
<tr>
<td>max. tolerable angle</td>
<td>(0.5°) (between absorber guide tube and connection rod)</td>
<td>1°</td>
</tr>
</tbody>
</table>
Both systems should function completely independent from each other and have the highest reasonable amount of reactivity worth provided.

The first 3 criteria of table 1, which are valid for the mechanical design of the KNK-1 system, are also valid for the SNR-300 system. Criterion 4 could be skipped since there was no need to care about an essentially completed plant design. Instead of that it was necessary to utilize the KNK-1 development work in order to minimize development risk and costs. Criterion 3 had to be extended in its substance to the actuating part of the plant safety system i.e. sensors and logic systems. The amount of shutdown reactivity and its rate of change has been derived from the fact of an integral positive void-coefficient of reactivity. The occurrence of a void accident seems theoretically possible during power operation or even in a shutdown condition, but this idea is certainly highly hypothetical since there is no obvious technical possibility either to transport a very large coherent bubble through the core or to produce such a bubble fast enough within the core or to maintain it in the core.

Disturbances leading directly to reactivity changes and being within the range of a considerable probability have been analysed also, but they are irrelevant from a reactivity worth point of view and rate of change as well. Only for the purpose of completeness criteria 7 and 8 from table 1 are mentioned. The concept license of SNR-300 has not changed these general design criteria, but only the special design criteria - and this process is still going on. The 2 shutdown systems have not been licensed as yet.

**Design (figure 2):** During normal operation each of the 3 units of the second system is kept in the lowest position below the core by the scram magnet thus tensioning the acceleration spring. In order to release scram the inductor current to the scram magnet will be interrupted. The released armature plate will be pushed upwards. The transfer rod connected to the plate now pulls the absorber in a fast stroke into the core.
After 80% of the liftstroke the shock absorption piston of the transfer rod enters the shock absorption cylinder herewith accomplishing a shock free retardation hydraulically. A spring installed in the upper part of the drive picks-up this function at the end of this process. Simultaneously with the scram release action the magnet follows immediately by means of the ball screw drive in order to pull any absorber which may incidentally have not completed its stroke fully upwards. During this operation the acceleration spring will be tensioned again. Since the armature plate now touches the scram magnet again, the absorber can be moved downwards into the lowest position after turning on the inductor current.

The absorber is a flexible chain of 3 rod bundles jointlyed coupled (highly enriched B₄C). Table 3 lists the distinguishing features between the first and the second system.

At the beginning of the refuelling operation connection rods have to be disconnected from the absorbers thus leaving the absorber chains below the core. They cannot be used during refuelling. This does not seem necessary, since this situation is properly backed up by a high degree of subcriticality of the reactor from the first system and by subcriticality monitoring during refuelling.

The degree of independence of the shutdown system of the KNK-1 has been extended to the total plant safety system. The design of the actuating part of the shutdown systems therefore has been based on the following principles: Considering the fact that all serious disturbances somewhere in the plant will finally change process parameters within the vessel and core region only trip signals from this region will be fed into the second safety system.

These process parameters are identical with some of those used within the first system. Herewith the second shutdown system can be considered as an increase in redundancy in accident protection. The detecting criteria have been selected in such a way that all design basis accidents can be detected diversely within each of both systems.
Table 3: Comparison Between the 1st and 2nd Shutdown System of SNR 300

<table>
<thead>
<tr>
<th></th>
<th>1st Shutdown System</th>
<th>2nd Shutdown System</th>
</tr>
</thead>
<tbody>
<tr>
<td>accident reactivity compensation</td>
<td>identical</td>
<td></td>
</tr>
<tr>
<td>sensing</td>
<td>diverse and redundant but different set-points</td>
<td>diverse and redundant</td>
</tr>
<tr>
<td>logic systems</td>
<td>different manufacturers</td>
<td></td>
</tr>
<tr>
<td>absorber</td>
<td>rigid rod bundle</td>
<td>flexible, acticulated 3 rod bundles</td>
</tr>
<tr>
<td>stand-by</td>
<td>above core</td>
<td>below core</td>
</tr>
<tr>
<td>insertion mode</td>
<td>free drop by gravity</td>
<td>pulled upwards by tensioned springs</td>
</tr>
<tr>
<td>scram release</td>
<td>indirectly by scram magnet opening a mech. scram clutch</td>
<td>directly by scram magnet</td>
</tr>
<tr>
<td>centering</td>
<td>centering tube held-down by spring forces</td>
<td>centering tube held-down by spindle</td>
</tr>
<tr>
<td>max. tolerable misalignment</td>
<td>(26 mm) (between axes of absorber and plug penetration)</td>
<td>40 mm</td>
</tr>
<tr>
<td>max. tolerable angle</td>
<td>(1.3°) (between absorber guide tube and connection rod)</td>
<td>2.2°</td>
</tr>
</tbody>
</table>
In order not to have the second system operate too often the set points of each system are different. This means that slow parameter changes will normally trigger only the first system, whereas fast transients will trigger both systems at about the same time.

In the PSAR the reactivity worth has been determined on the basis of the maximum void reactivity for both systems including all calculational uncertainties for the first but not for the second one, since a maximum void situation seemed too hypothetical anyway.

From this same reason and from the fact that both systems act completely independent with about the same reactivity worth installed another conclusion has been drawn, namely not to take into account a stuck-rod in either one system as it is demanded for example in the 10CFR50, App. A of the USAEC-General design criteria. The assumption of a malfunction of one complete system goes already far beyond this stuck-rod criterion. This is, of course, only valid for the maximum void situation, since for all more probable situations, for example accidental withdrawal of all absorber rods, a stuck-rod could easily be tolerated. Only to demonstrate the capabilities of either one system in terms of rate of change a stepwise entrainment of a bubble worth about 1 % and a 5 %/sec ramp rate have been analysed. The analyses showed no intolerable damage to vital core components, and although no diversity in trip signals could be established there is still redundancy.

The requirements stated by the authorities so far are essentially the following:
- include the calculational uncertainties for the reactivity worth in both systems
- provide diverse logic systems within both safety systems which have to be manufactured by different suppliers. The systems must not have elements of the same manufacturer.
- increase the redundancy within the trip units of either one system and use relays of different manufacturers
- the second shutdown system has to be able to handle the differential deformation arising when the reactor runs from the set points of the first system to those of the second one.
These requirements have been partly fulfilled, some are still under discussion, therefore no special comments to these will be given at this time.

2.3 KNK-2

Requirements: Although the basic idea to change the KNK-1 into a test bed with a fast core had been existing for quite a while - it had developed during the development work for SNR-300, but had already been considered during the design work of KNK-1 - a revised PSAR was submitted to the authorities not before the end of '72, about 2.5 year later than for SNR-300. The change from KNK-1 to KNK-2 has been considered so important by the authorities that they subjected it to a new licensing process according to the German Atomic Energy Law.

The nature of the accidents to be considered is quite similar to the SNR-300 situation even though the energy release potential is very much less. Thus the requirements related to the safety system also had to be similar and had to be incorporated into an existing plant. Since the plant had not been designed against the consequences of a failure of shutdown systems, the strongest effort had to be concentrated on preventive measures. The authorities postulated a "slumping"-accident inside one fuel assembly as a design basis for these preventive measures.

The specific requirement for this accident is to insert reactivity as fast as fuel slumping by gravity can change reactivity (1). A further requirement is to cut the connection between the first and the second system and actuate the second one independently from the first one (2).

Design: The design of the mechanical parts of the second shutdown system has been changed a bit when compared to the KNK-1 system. The reason for these minor changes were due to the necessity to install more absorber material.
The "tube-section" chain has been turned into a "rod-bundle" chain and the shock absorption is accomplished by a similar piston system as in the case of the first shutdown system. This means that the displacement rod has been eliminated. The overall design, however, is still similar to the KNK-1 design.

With the maximum possible amount of absorber material installed it turned out to be impossible to meet the 1st requirement in both systems. Only the first shutdown system can do this via the signal from the reactivity meters but without a licensable margin. The authorities therefore required that the second shutdown system has to be actuated at the same instance in order to provide the necessary margin in case of this highly hypothetical accident situation. To fulfil this requirement - the discussions are still going on - one would have either to connect the reactivity meters of the first system into the safety circuits of the second one or provide separate reactivity meters. Both solutions have a considerable disadvantage in that the second system would be actuated with each reactor shutdown, since the reactivity change during shutdown is large enough to override the set points of the reactivity meters. This is not desirable because the number of shutdown actions is rather limited from a mechanical design point of view. To overcome this problem one has either to live with this limited-number situation and eventually install new shutdown rods or to jumper the reactivity meter signals in all cases where the scram is initiated by other faults. This, of course, poses another safety problem.

On the other hand all rather probable accidents can be handled with both systems independently. The basic principle of selection of trip signals is identical with the SNR-300 concept.
3. Experience and Test-Programs

3.1 KNK-1, Out-of-pile Performance:

In chapter 2.1 some highlights have been given to the first steps in the experimental development of the second shutdown system of KNK-1. The results of any single step have led to important design changes. The essential parts of the finally selected solution have been tested individually, for example:
- experimental optimization of the scram magnet
- relaxation tests of the Belleville springs
- tests of the drop and shock absorption performance of the absorber chain, i.e. registering of the travel vs. time and acceleration vs. time function of a dummy in a water test rig. The hydraulic shock absorption has been improved by changing the respective geometrical conditions.

The tests with a complete drive unit have shown deficiencies of the torque clutch and of the performance of the motor during starting and run-down, which have lead to respective changes in mechanical and electrical design. The performance test of the complete prototype unit has been run in a test section filled with water as a test fluid. The following duration test (100 scrams and 400 double strokes) has been performed in a test section connected directly to a Na-test rig under conditions similar to the reactor environment. All 3 final complete shutdown units have undergone 10% of the number of actuations expected during reactor operation in the Na-test section before installation into the reactor.

During these tests the leak rates of the drive units and of the connection rods have been measured by means of a He-leak check equipment. In the water and sodium test sections the immediate surroundings of a shutdown rod has been built-up so that radial misalignment between the axes of the drive unit and the absorber guide tube could be simulated. A part of the tests have been performed with a misalignment of about 40 mm. Herewith a core misalignment relative to the rotating plug of about 20 mm has been simulated, which has been a design target together with another 20 mm resulting from a thermal bowing of the connection rod.
The following essential items have been measured during these tests:
- during lifting and lowering of the absorbers:
  - the torque momentum at the ball screw
- during scrams:
  - the dead time and the total scram time via a time-point measurement from the moment of the release, the opening of the absorber clutch and the touch-down of the absorber in its lowest position
A number of deficiencies has been observed during all these tests:
- gas leakages at the drives and the connection rods
- jamming of a connection rod with a displacement rod due to a fabrication error
- spurious scrams due to a wrong adjustment of the absorber clutch claws
- torque moment peaks due to a badly fabricated ball screw
- jamming of the absorber with the central displacement rod within the shock absorption section
- loss of the leak tightness of the gas seal in the upper connection rod area resulting from sodium deposits due to the up- and down-movement; this effect has been eliminated by adjusting a downward flow of a gas flush within the respective annulus.

After dismantling the shutdown units from the test sections and after partial disassembly of the rods fretting and wear due to unfavorable combination of materials and too narrow gaps were observed. All these deficiencies have been eliminated by proper measures, the efficiency of which have been experimentally proved. Finally, after a re-assembly of the units dry functional tests have been performed in order to assure a delivery of sound and proven equipment.
3.2 KNK-1, In-pile Experience

After installation of the 3 shutdown rods into the reactor compliance tests were performed under the observation of the local authorities in 1971. These tests comprised essentially the following single measurements:
- leak rate
- friction forces between the connection and transfer rod
- shutdown time with accelerometers at the reactor tank flange

In intervals of about 3 months recurring inspections have been performed. The absorbers and the connection rods were placed into the stand-by operating position after they had been in the lowest position during the whole shutdown period for quite a while. Then a scram signal was simulated. The shutdown time was determined under similar conditions as during the compliance tests, i.e. a system temperature of about 200°C. In April '73 shutdown times were measured and found to be considerably longer than usual. Instead of about 660 ms about 1500 to 2600 ms were found. The reason was that a liquid sodium film inside the annulus between the lifting and release rod was transported together with the rods during the lifting movement into a colder zone of about 100°C where the sodium had frozen. Therefore the mobility of the release rod relative to the lifting rod during scram action had been impaired. By increasing the temperature in the respective rod area by means of a modified operation of the plug cooling system this problem was solved as the follow-up inspection tests have shown.

This problem had not been identified during the out-of-pile tests with the prototype and the 3 final rods because the temperature in the critical rod area had not been simulated properly.

3.3 Proposed tests for the KNK-2 rods

Since the KNK-2 unit shows some minor mechanical design changes when compared with the KNK-1 unit, the following tests have been proposed:
- pre-tests with the novel parts
- functional tests with a prototype within a water test section
- functional and endurance tests with a prototype within a sodium test section
- functional tests with the 3 final units:
  1. 2 units within the water test section with a vacuum-distillation thereafter
  2. 1 unit within the sodium test section with following complete disassembly in order to check on fretting and wear, then a functional dry check

It is obvious that all experience gained from tests and operation of the KNK-1 units will be fed into these tests for KNK-2.

3.4 Proposed tests for the SNR-300 units

The experimental development program for the second shutdown units of SNR-300 will be divided into individual steps similar to the KNK development work. There are components of the program, however, which need to be mentioned because they go beyond the scope of the KNK-work:
- additional, rather extensive instrumentation of the prototype unit in order to get a better knowledge of the behaviour of the rods (for example travel vs. time, acceleration vs. time characteristic under sodium conditions)
- simulation of core deformation and misalignment
  1. installation of bowed guide tubes
  2. establishment of extensive misalignment between plug and core.

Prior to performing these tests all programs have to have the concurrence of the authorities.

4. Résumé

One might argue that the degree of diversity between the two systems in any one of these reactors is possibly not as high as it might be desirable. This situation has two basic reasons:
1. The second shutdown system was started as a mechanical type, since it was impossible to find a system of a higher degree of diversity - as outlined for KNK-1 in chapter 2.1 - under the given conditions. Therefore the KNK-1 design has been optimized for KNK-2 and SNR-300, last not least in order to cut down on development cost and risk.

2. Knowing that the degree of mechanical diversity is rather limited studies have been done on a more or less calculational basis to find a different solution. It turned out, however, that the liquid absorber material could not fulfill the reactivity requirements neither with respect to the reactivity worth nor its rate of change. At that time the disadvantage of such a high reactivity requirement, generously accepted earlier, has become obvious. There are other rather serious disadvantages, for example reduction in breeding gain due to the core positions needed for absorber rods, thermal shocks during shutdown action and, last not least, the theoretical potential of a very serious rod ejection accident. So one has to pay a high price for the coverage of extremely hypothetical accidents by the shutdown systems. But the reactivity question was not the only disadvantage of the liquid poison system. As far as studies can go it could be shown, nevertheless, that the engineering and operation of such a system is highly complicated and therefore more subject to malfunctions.

From these findings discussions have come back to the essential question: what is better diversity at any price or overall reliability? Since nobody has exactly defined so far what sort and size of core deformation could arise, the answer to that question was to stick to the mechanical type and improve it as far as possible in the direction of diversification and independence from the first system. This has been done. Based on an experimental and theoretical understanding of what may occur in a single subassembly event, it is highly unlikely that there could occur a simultaneous deformation of shutdown rod guidetubes with the consequences of the rods not being able to enter the core to shutdown the reactor.
First Shutdown System
indirect release:

- scram magnet
- mechanical clutch
- rotating plug
- sodium level
- shock absorber piston
- rigid absorber rod (rod bundle)
- core

Second Shutdown System
indirect release:

- scram magnet
- mechanical clutch
- articulated absorber rod (pipe sections)
- shock absorber piston

KNK-1: SHUTDOWN SYSTEMS
First Shutdown System
indirect release:
- scram magnet
- mechanical clutch

Second Shutdown System
direct release:
- scram magnet

rotating plug
sodium level
shock absorber piston
rigid absorber rod
(rod bundle),
above core region
articulated absorber rod
(3 rod bundles),
below core region

SNR-300: SHUTDOWN SYSTEMS

FIGURE 2
CONSIDERATIONS RELATIVE TO A BACK-UP SYSTEM

by MM. P. CLAUZON - P. MARBONIER - A. MEYER HEINE - J. LADET (EEN CADARACHE - FRANCE)
P. CACHERA (LDF - Electricité de France)
J. DELEMONTEY (GAIA - Groupement Atomeque Alsacienne Atlantique)

SUMMARY:

The interest of a back-up system is analyzed. In the first part a description of the classical safety system of Phenix is given, then the specifications of a back-up system are listed in a second part. Different possible solutions are examined.

I./ INTRODUCTION

The classical security system of a reactor is composed of three links:
- the detection function is made up of the sensors, and amplifiers.
- the command function includes the 2-out-of-three logic system, the safety logics and the electromagnets of the control rods.
- the shut down function is formed by the control rod system.

To be sure that the reactor can be stopped for a given incident, one must then be sure that the incident can be detected, that the order of shutting down the reactor goes to the control rods and that the control rods actually drop.

There are two possibilities to increase the reliability of the security system:
- to increase separately the reliability of each of the three links: detection, command, scram.
- to decrease the number of links and have, for those incidents which are assumed to be the most important, a security system which would be set into action by the actual physical cause of the incident. In such a way one is freed of any risks of default in the detection system or in the command system.

The purpose of this study is to examine the eventual interest of a back-up system.

In the first part, an analysis of a classical security system, directly inspired by Phenix, is made. In the second part one gives the specifications of a back-up system. After examining and comparing the various technical solutions, some indications are given on different solutions.

In any case one must underline that this work only presents the state of the art at the present time.

II./ THE CLASSICAL SECURITY SYSTEM

II.1.- Description

Fig. 1 shows an outlook of the Phenix security system.

1.- Detection function

The measure of a physical parameter is made most of the time by these sensors each of which being linked to an amplifying chain. These three chains are then used in a two-out-of-three logic. Each incident is detected in two different ways (physical redundancy). For instance a total primary loss of flow can be detected through a flow measurement or a temperature measurement.
2. Command function

The 6 control rods of a reactor such as Phenix are divided into two groups which are set into action in a completely independent way.

Fig. 1 shows that for Phenix the whole part of the command function is doubled: safety logic, circuits and electromagnets. It is obvious that, as far as the circuits are concerned one must furthermore be sure there is no meeting point.

3. Shut down system

For Super Phenix there are 21 control rods and only a third of them is needed to turn the reactor from its full power to an undercritical state.

Merely a carbon copy of Phenix, these control rods are used for the three following functions altogether: safety, fuel compensation, power and temperature countereactions.

These mixed-function control rods present, as far as safety is concerned the following advantages:
- the safety margin is spread in every control rod,
- because of their compensation role, they are often used. Any risk of blockage through impurities deposits is in fact eliminated.
- since the control rods are being constantly inserted in the reactor (disregarding the end of fuel life) they are more effective, in a scram, than a safety control rod located, in principle, outside of the core.

For the control rods of Phenix, experiments have shown that an important unalignement between the axis of the guide tube and the axis of the sheath can be accepted. In the same way for Super Phenix an unalignement, at the grip level, should not prevent the control rod from dropping.

II.2. Analysis of the causes of malfunctioning

The causes of malfunctioning of the detection and command system will not be examined in this paper. It is believed that in most of the cases it will be possible to make them reliable.

* Detection system: by increasing for instance the number of the sensors, so as to be sure that any incident can be detected through more than two independent ways.

* Command system: by not only doubling the circuits, but by making sure that they present no connecting point throughout the whole system. It must be possible then to guarantee that there will be no possibility of a common default in this link of the security system.

On the other hand, it is essential to look very closely at the causes of malfunctioning of the shut down device itself.

If one leaves out the possibility that a great number of control rods can fly off during a loading or un-loading period, one can only list two causes of malfunctioning for the arresting device:

a) The absorbing material and the mobile device can be jammed, during their fall, either by sodium impurities or by differential swelling between the guide-tube and the sheath of the rod. It is not possible that such a defect can touch more than 2/3 of the rods. The constant moving up and down of the rods (fuel compensation) not only allow the detection of blockage due to deposit of impurities, but furthermore detect any other type of blockage through abnormal efforts to set the rod into motion. Besides, the program for the survey of the control rods and the experience we will have from Phenix should prevent any incident connected with the swelling of the structures of the rod.

b) In case of a deformation of the structures of the reactor leading to important unalignement between the axis of the guide tube and the axis of the sheath it is quite obvious that a defect during the drop of the rod could be feared. This defect can touch all the rods. Anyway it has to be noticed that in this case, if the connection between the mobile device and the rod is cut off, one can hope that the rods will be inserted, at least partially, inside the core.
In fact, and this point has to be emphasized, one situation only, can prevent the security system from operating: if the structures of the reactor are out of shape. The possibilities of such incidents must be looked over very closely.

II.3.- Accidents which could lead to the deformation of the structures of the reactor

Only four accidents can lead to important deformations.

1.- Seism

The whole system is built so as to resist to a seism of intensity 7, Mercali scale, and to keep working the security function in case of a seism of intensity 8. This imposes a checking so that a seism of intensity 8 does not lead to relative displacements between the rotating plug and the control plug, thus preventing the dropping of the rods. Or, if this is impossible to check, it must be proved that the rods have the time to drop before the deformations become too large, the complete time between the arrival of the order to the end of the fall being about 1 second.

One recalls here what has been specified for Phenix.

- The devices of the rods are specified so as to be able to drop in case of a seism of intensity 8.

- The structures of the reactor are specified so that, in case of a seism of intensity 8, the displacements do not prevent the rods to fall. There should be no rupture of any important components (suspension of the vessels, connection between the roof and the top, diagrid, etc...)

These specifications will still be valid for Super Phenix. They guarantee, on the first hand, that in case of a seism of intensity 8, the rods can drop, and on the other hand that no important components are hurt.

2.- Blockage of a subassembly and explosion of the subassembly after fusion

This accident could also lead to a damage of part or the whole of the reactor. An experimental program will allow us to determine the consequences of such an accident. Anyway it can be said that this accident would probably be detected while it is happening and before the explosion of the subassembly.

One believes that this accident would not prevent a partial fall of the rods.

3.- Rupture of the diagrid

This accident is not a primary accident. It leads to a reactivity insertion due to extraction of the rods from the fallen part of the core.

4.- Accidents due to missiles from out of the reactor building

This accident should be detected by seismographs. It can be assimilated to a seism, the drop off point of the missile being the seism epicenter.

II.4.- Justification of a back-up system

From the analysis carried out in II 2 and II 3 it appears:
- the two first links of the security system (detection and command) can be made reliable by appropriate means.
- the last link, that is to say the arresting device itself is reliable, subject to:
  - seism specifications are identical for Super Phenix and Phenix.
  - confirmation with an experimental program on subassemblies explosion that the deformations do not affect too many rows of subassemblies.

Anyway, one has the feeling that this last link may have a common defect, because the rods are all conceived on the same model and have the same way of insertion inside the core.
This common defect could appear either because of important deformations, or because of a missile cutting all connections between the heads of subassemblies and the control plug.

The justification of a back-up system may thus appear. Its two essential characteristics would then be the following:
- on the first part, a back-up system is quite different in its principle and its location from a classical system.
- on the second part, a back-up system must cover a range of accidents which could lead to important structural deformations.

A question has yet to be answered: should a back-up system act in addition to the classical system and therefore be disconnected for each scram or should it be only disconnected for specified accidents. Depending on the answer, it is clear that the technical solutions and the consequences on the safety of the reactor would be different.

It has to be stressed that a solution leading to a disconnection of the back-up system at each scram and for specified incidents would present two advantages: doubling the safety function and parting the security margin between the two systems.

Before examining the different possible solutions one cannot help to underline that a back-up system will increase the complexity of the reactor and will be the cause of unexpected scrams.

This inconvenient must be counterbalanced by the fact that it is most improbable that the control rods would not disconnect in case of any accident. At most, the probability of such an event would be so low that it would be possible to run the risk and not to consider it in the list of accidents.

III. BACK-UP SYSTEM

The specifications of such a system and the different possible solutions are analyzed hereafter.

III.1. Specifications

1. The back-up system is only used for safety. So as to simplify the design and to avoid the presence of absorbing material in the core during normal steady power operation, the back-up system will not be used for fuel compensation or reactor control.
2. Response time
   The back-up system must have characteristics not too different from those of a classical system. That is to say, a time lag of about one second.
3. Antireactivity
   Enough to set the reactor cold undercritical, this reactivity margin is added to the reactivity of the classical system.
4. The antireactivity of the back-up system needs not to be inserted during loading or unloading period.
5. The back-up system must be based on a different principle than the classical one. It must not include vital components between the head of the subassemblies and the control plug. At least, it should not prevent the insertion of antireactivity.
6. The device must be reliable not withstanding important reactor-components deformations, corresponding to a seism of intensity higher than 8, Mercali-scale.
7. The system must be disconnected automatically
8. Location
   The system must not disturb the power flattening control. For Super Phenix there are three possible emplacements: the inner rod row, the outer rod row or the radius at mid-distance between the two rows.
9. Reliability
   The system must be reliable and must be easily checked during normal unloading periods (that is to say once or twice a year). An unexpected discon-
nection must not lead to heavier consequences than an unexpected disconnection of the classical system.

10.- This last specification is an eventuality: the back up system doubles the complete security system (detection, command, shut down) in case of a coolant coast-down. The physical cause itself would then automatically disconnect the back-up system without interference of the normal detection and command circuits. No need to say that this condition will be fulfilled with difficulty and implies a complex device.

Besides it would be convenient that:
1. The back-up system would not lead to large modifications of the reactor concept.
2. The immobilisation of the reactor due to a back-up disconnection must be as low as possible.
3. Its volume in the core must be as reduced as possible.

III.2.- Possible solutions

They can be classified following the nature of their action a) insertion of absorbers b) extraction of fuel c) displacements of reflectors d) global modifications of core geometry and the mode of their action e) mechanic f) hydraulic g) thermal h) pneumatic.

Must be eliminated:
1. Possibilities d) and c) leading to too important movements of fuel
2. Possibility b) : a fuel element located in the core during normal operation should be replaced periodically as a normal subassembly. Furthermore its cooling would be difficult to realize.
3. Possibility g) : would lead to too important a time constant. Such a system seems difficult to test or to be automatically disconnected
4. Poisoning of the core with a liquid solution of lithium in sodium. The quantity of lithium would be too large. A period of 22 weeks being necessary to start up the reactor, an automatic shut down of the reactor would be difficult to impose: specification III.1.7 is thus not fulfilled.

Finally three types of solution can be proposed:
1. Liquid absorbers in a closed loop
2. Solid absorber of a chain type set into action through a mechanical device.
3. Solid absorber, divided or not, hydraulically or pneumatically moved.

III.3.- Description of the possible solution

1. Liquid absorber (Li 6)

It can be very easily adopted to eventual movements of structures.

There are in any case certain inconvenients:
- Its efficiency is lower than the boron carbide: 1/4 of the efficiency of B4C for the same volume for highly enriched Li 6.
- There is production of tritium
- It melts at 180° C and vaporizes at 1317° C
- It has a high corrosion power on stainless steel

Figure 2 shows such a system. During normal operation the Li 6 is held in a capacity with a cover-gaz. The subassembly is filled with gaz and in communication with a capacity under pressure. The lithium is pushed into the core if the pressure is isolated.

It is necessary to hold the lithium either directly under the subassembly or outside of it (the insertion being possible through the bottom-spike of the subassembly); both solutions are shown on fig. 2.

It should be noted that:
- Holding the lithium in the upper part of the subassembly means a non negligible production of tritium due to the presence of a high flux.
- Holding the lithium outside of the subassembly means a feeding through the bottom spike and thus a problem of tightness to prevent the lithium from leaking into the sodium.
2. A chain system should be preferred to spherical particules absorbers because it is easier to arm and disconnect automatically.

A preference toward B4C appears in France because of its high efficiency and its proved technology; besides tantalum presents a high residual power, but on the otherhand tantalum does not procedure helium.

Fig. 3 shows such a system. The absorber is maintained under the plug with an electromagnet. The shutdown results of a disconnection of the electromagnet feeding. A spring pulls the absorbers downwards so as to have a shorter time-drop.

This device can be completed by adding a piston at the lower part of the chain. In case of no pressure in the diagrid the fall is automatic. One just has to realize an equilibrium between the spring and gravity pulling downwards, and the electromagnet plus the pressure pulling upwards.

Note that the connection between the plug and the subassembly is not vital and any disconnection means a drop of the absorber.

If full flow is not established (starting of the reactor or handling period) the electromagnet force has to be increased. To forget to reset it to its nominal value during normal operation would mean the chain would only drop if there is a disconnection of the electromagnet but not for an underpressure in the diagrid.

3.- Absorber set in motion through hydraulic or pneumatic devices. Such a pressure can act on a piston as describes § 2. The pressure cannot be the diagrid pressure if the system is supposed to work each time the classical rods are in action. Therefore, there must be an auxiliary force (electromagnetic pump) which is disconnected when the system acts. Fig. 4 shows a schema of such a solution, the pressure being carried on through the head or the bottom of the subassembly. In the latter case the subassembly does not need to be unloaded from the reactor during the handling periods as there is no connection between the top plug and the subassembly.

4.- A special mention must be about an original system each safety element is composed as the normal fuel by pellets enclosed in a clad. But the inside hole has much a section that in case of fusion it can fall by gravity to the bottom of the element. Fig 5a, b and c show the cross section of a normal element and of two versions of this solution.

This device can only act after the negative temperature coefficients. It only reinforces a counteraction which has not been important enough to avoid a fusion. Such an element does not answer all the characteristics of a back-up system but can reduce the consequences of an hypothetical core fusion.

III. 4.- Comparison of the three solutions

Solution (2) has the following advantages:
- no important modifications of the core design
- an electromagnet working in sodium has been tested

But it presents the inconvenience which is the need to extract the command system of the electromagnetic during the handling periods. Furthermore the rearming of the control rod must be carefully looked upon so that the device will be simple enough.

Solution (1) (liquid absorber hydraulically set into motion) described in III.1. presents the advantage of an absorber with a shape able to accommodate large deformations.

There are however the following inconveniences:
- it necessitates either a volume of Li 6 located in a zone with a high flux, which involves a tightness problem (tritium) or a circuit for the arrival of Li 6 under the diagrid, which involves a tightness problem at the bottom spike.
- the whole device has to be taken away during handling periods.
- need for a gas storage capacity and most probably for a circuit under the diagrid.
- non negligible research program because Li 6 is not a material
frequently used in fast reactor.

Solution (3) (solid absorber in chain or in spherical form, hydraulically started) does not present any advantage as long as it is desirable to have a system working for any incident and not only in case of low pressure under the diagrid. But it presents some disadvantages:
- the need for an electromagnetic pump
- the need, either for an inflow circuit under the diagrid (the whole system stays in place during the handling periods) or an inflow circuit over the head of the subassembly (which must be extracted during the handling periods).

IV. CONCLUSION

This short analysis shows that an articulated solution (2) answers most of the criteria asked of a back-up system and that its overall dimensions do not sensibly modify the structures of the reactor. In any case, it has to be stressed once more that there appears no clear and easily defined accident which would necessitate the presence of a back-up system. Its main usefulness is to decrease the probability of a nonshutdown of the control rods which would eventually mean that such an accident does not have to be taken into account in the final safety analysis.

CLASSICAL SAFETY SYSTEM

![Diagram of Classical Safety System]

Fig. 1
LIQUID ABSORBER

BY-PASS

VALVE OPEN:
LITHIUM SHUT DOWN

STORAGE
LITHIUM

GAS
CORE

GAS
Fig. 2

SOLID ABSORBER. MECHANICAL MOTION

PRINCIPLE OF AN AUTOMATIC SHUT DOWN IN
CASE OF A LOW PRESSURE IN THE DIAGRID

COMMAND OF THE
ELECTROMAGNET

ELECTROMAGNET
HEAD

ABSORBER
ARTICULATED SHAPE

CORE

SPRING
PISTON

Fig. 3
Fig. 4 ELECTROMAGNETIC PUMP SAFETY ELEMENT WITH A LARGE CENTRAL HOLE
LMFBR ALTERNATE SHUTDOWN SYSTEMS --
TEST EXPERIENCE AND GENERAL DESIGN CRITERIA

R. C. Noyes
and
J. C. Gilbertson

FBR Development Program
Nuclear Power Department
Power Systems Group
Combustion Engineering, Inc.
Windsor, Connecticut

ABSTRACT

A brief review of the history of alternate shutdown systems for LMFBR's is presented together with a discussion of the rationale used in developing six proposed general design criteria for such systems. A reactor shutdown system developed at Combustion Engineering, Inc. is described which uses a modified EBR-II type control rod system with a redundant second system similar to FERMI safety rods except that the scram mechanism is pneumatically actuated. Test results of a model of the pneumatic scram release mechanism operated in sodium at 1100 F are given.

INTRODUCTION

An alternate shutdown system is a redundant system for reducing reactor core reactivity which operates on mechanical principles different from the reactor's other "partner" reactivity shutdown system. For example, a light water reactor may be equipped with a system of scrammable control rods; its redundant system could be a system for introducing soluble boron into the primary coolant. If one system fails when needed, the other shutdown system retains a high probability of shutting down the reactor because its operating principle is different from the partner system and, therefore, not subject to the same common failure mode.

Use of alternate shutdown systems, an idea extending back to CP-1, is generally accepted as good practice for all reactors. It is an explicit requirement for licensing light water reactors in the U.S.; however, this design rule has not been applied universally to all reactor types.

Past LMFBR designs in the U.S. have a wide range of shutdown systems. Of the three LMFBR's licensed in the U.S., FERMI had no fast acting second shutdown system (alternate principle or otherwise), EBR-II has two mechanically actuated movable fuel systems of different principle, and SEFOR has a
movable reflector system combined with mechanically actuated poison rods. FFTF has no alternate principle, but has two independent, identical systems. The U.S. LMFBR demonstration plant designs proposed in 1971 show no commonly agreed-on design criteria. The designs proposed by Combustion Engineering, Inc., Atomic International Inc., General Electric Co., and Westinghouse Corp., all have two independent shutdown systems; however, only two designs, C-E and GE, have an alternate principle for the second shutdown system. It is understood that the Clinch River plant will have two independent systems of poison rods, one mechanically actuated and the other actuated by some other principle not yet fully defined.

For the sodium cooled fast reactor, the central problem has been the development of a shutdown system which is alternate to conventional control rods, which is practical, and which satisfies the experts that the proposed system is really a system of "different principle." Various liquid lithium systems have been suggested which have had little difficulty passing the different principle criterion, but have been judged to be impractical for various reasons. The same is generally true for movable reflectors, hydraulic or gravity actuated poison balls, and other radically different concepts. The systems that are usually judged to be practical for LMFBR's are vertically moving poison rod systems or vertically movable fuel systems superficially similar to conventional control rods. These systems are not obviously different in principle from conventional control rods as, for example, a liquid lithium system is.

In contrast to mechanical actuation of conventional control rods, proposed alternate control rods are actuated by means other than mechanical (hydraulic, pneumatic, or electrical) or, if by mechanical means, by mechanical means different in principle from the mechanical means in the partner system. Also, the poison assemblies or the shrouds of proposed alternate systems are often designed differently with the objective of minimizing the possibility of jamming due to in-core distortions or blockages. Such proposed systems contain differences of mechanical principle which are in the details of design rather than in the overall system configuration. For these systems, the question is whether or not such differences of design detail provide the added safety which is the objective of an alternate shutdown system.

DESIGN CRITERIA

The first advantage of using systems of different principles is that this approach is likely to prevent systematic design, manufacture, or operating error from rendering the entire shutdown system useless. Different principles tend to lead to differences in the personnel and methods used to design, fabricate, test, maintain, and operate the systems. These circumstances yield a minimum probability that human lack of knowledge or human mistake will lead to a common mode failure in both shutdown systems at the same time.

This aspect of the safety advantage obtained from using different principles is most difficult to define explicitly in engineering terms. It is a design concept aimed at an un-
known future circumstance resulting from human error or ignorance. Experience has shown that this idea works in practice; how it works in any particular case can only be determined after an incident has occurred. Consequently, only general qualitative criteria can be used in judging the value of a proposed combination of partner shutdown systems relative to this aspect, i.e., protection against human ignorance or error. A criterion of the following general nature is suggested.

1. **Human Factors Protection.** To evaluate the degree of protection, consider questions such as:

Design: Are different design disciplines required for the two shutdown systems? Are key components in the scram train different? Are the fail-safe and backup insertion features different?

Fabrication: Are fabrication methods different? Are different materials required for key items? Are key components supplied by different manufacturers?

Operation: Are the drives and in-core assemblies handled differently during refueling? Does the energy required for scram come from a different source? Is operational testing done differently?

Maintenance: Are different maintenance skills required? Are maintenance procedures different? Are periodic replacements of components done at different times? Is it possible to replace a common function component of the partner system with the same component?

The second advantage that is gained from using two systems of different principle is that, if properly designed, each system will complement the other relative to the partner system's inherent limitations or vulnerability. All LMFBR's have,

(1) a high neutron flux, high temperature sodium environment in which the control elements must operate, and

(2) a long distance and complicated interfacing between the control elements in the core and the control drives and release mechanisms above the reactor head.

These conditions lead to what may be called drive-line vulnerability and in-core vulnerability. If scram requires a long drive line with many points of possible interference, then such a system is vulnerable to jamming due to local faults or due to a common fault such as accidental rotation of a refueling plug. The environment in the core region is sufficiently unfavorable so that one must assign a significant probability to the possibility of jamming due to distortion, or foreign material, or failure after extended exposure of mechanisms near the core.

A third category of vulnerability to common mode failure can be called x-reactor vulnerability. This is not unique to LMFBR's but refers to possible sources of common failures such as building fires, explosions, earthquakes, storms, and dropped equipment. It is clearly desirable that differences in the two shutdown systems lead to decreased overall system vul-
nerability to these potential sources of common failure. Since this is a common situation for all reactor types, this requirement will not be discussed in any further detail here. Our second general criterion for LMFBR alternate shutdown systems can be stated as follows:

2. Inherent Vulnerability Protection. The two shutdown systems should be designed such that each is least vulnerable where the partner system is most vulnerable to potential common sources of failure. For LMFBR's, particular attention should be paid to drive-line vulnerability and in-core vulnerability.

The above two criteria are fundamental in considering LMFBR alternate principle shutdown systems. Following are more specific criteria which are suggested to complete the principal requirements for these systems.

3. Speed of Action. Each shutdown system should be fast acting. Based on studies of large primary pipe rupture accidents, a release time of 0.1 second accompanied by a 1.0 gravity acceleration is sufficient response speed to provide protection from gross core damage for the most severe postulated accidents.

4. Reactivity Requirements. Each shutdown system should be independently capable of covering structural, Doppler, and other temperature and power related reactivity contributions from full power to a cold standby condition. Additional negative reactivity is required in each system to cover the maximum worth of one shim rod and to provide an appropriate shutdown margin.

5. Testability. Each shutdown system must be easily testable on a regular basis to prove its operability.

6. Reliability. Each shutdown system should be of equal proven high reliability. The probability of failure to scram when required should be of the order of $10^{-5}$. Under no circumstances should a system of lower reliability be considered in order to obtain a radically different principle of operation.

Figure 1 illustrates schematically the C-E LMFBR safety system based on the above described criteria. This safety system combines a scannable shim-regulating rod with an EBR-II type scram actuation and an independent partner shutdown system employing a pneumatic scram release mechanism located immediately above the core. The alternate shutdown system is similar in mechanical principle to the FERMI control rod design except that, in the C-E design, the drive shaft scram actuator of the FERMI design has been replaced with a bellows sealed pneumatic actuator. Details of concept selection, design, fabrication, and successful sodium testing of the pneumatic latch are described in following sections.

It is important to emphasize that no distinction is made in safety performance requirements (insertion speed, reactivity, reliability, etc.) between the primary shutdown system and the alternate shutdown system. This is a key implied criterion contained in all of the six criteria stated above. An alternate shutdown system should not be developed or evaluated in isolation from its partner system. Each system should complement
the other; the different principle of operation must be judged relative to the partner system; and each system must be independently capable of shutdown response that will prevent gross core damage for the most severe accidents.

CONCEPT SELECTION

In selecting the general features of the safety system, first, a review was made of shutdown systems that have been developed, fabricated, and installed in operating LMFBR's. This is a good way to find practical systems that are of proven high reliability (criterion 6). A review was also made of various alternate shutdown systems of unconventional design from various sources. Many were judged as failing to meet at least one of the above criterion, usually one of the last four: speed of action, reactivity, testability, or reliability. Those that did appear to have the potential to meet all basic criteria presented the prospect of very costly and time consuming development programs to prove practicality and reliability. It was decided, therefore, to concentrate on modification of systems already proven practical and reliable.

Potential problems associated with the high neutron flux, high temperature, sodium environment can be minimized by using an EBR-II type design which places the scram release-insertion mechanism outside this environment and connects the control element to the release-insertion mechanism with a strong...
shaft. In addition to putting the scram mechanism in a favorable environment, this design provides for the application of a positive insertion force to make sure that control element insertion is not prevented by obstructions in the core. Thus, the gas accumulator system is used to overcome resistance to insertion rather than for acceleration to achieve high insertion speed. Reactivity reduction in the C-E system is obtained by the use of poison insertion rather than fuel movement in order to avoid the problem of cooling when a movable fuel system is used.

Years of operation of EBR-II have proven this type of system to be practical and reliable; however, it is potentially vulnerable to drive line jamming.

To eliminate the possibility of drive line jamming, an alternate shutdown concept was developed based on the proven FERMI design in which the safety rods were suspended from a scram latch situated just at the top of the core structure. The latch is mounted at the lower end of a proven control rod drive mechanism and actuated by pneumatic means. The pneumatic mechanism replaces the mechanically actuated latch release of the FERMI design in order to obtain complete immunity from mechanical jamming of any kind in any part of the drive line. An electromagnetically actuated latch was also studied and judged to be a probable practical alternate but to require considerably more development effort. Hydraulic devices were judged relatively complicated but potentially fast.

Although the pneumatic latch mechanism is the primary feature of C-E's alternate shutdown system design, other features were incorporated in order to improve the probability of successful shutdown under all adverse conditions. One such feature consists of the provision of a flexible poison element which is less likely to be restrained during insertion into the core by a bent or deformed safety rod duct.

To achieve this the design problem was narrowed to selecting a poison element design which would pass through a curved and/or buckled duct but would not itself buckle and jam due to the down thrust of the scram assist spring. These restrictions led to a central spine to take the dynamic, compressive spring thrust and various means of flexibly trailing the pins from its leading end. This rapid convergence to a conventional absorber geometry, with some modification for ease of insertion, represented a determination to retain as much proven hardware in the design as possible.

**DESCRIPTION OF ALTERNATE SHUTDOWN SYSTEM**

The alternate shutdown system is shown in Fig. 2. It consists of a control rod drive mechanism (CRDM) above the reactor vessel head, having a drive extension connecting it to the safety rod inside the reactor core. The pneumatic latch release system is incorporated into the CRDM ball screw and drive extension which have a pair of fast-acting solenoid valves at the upper end, a connecting fill/blowdown passage through the center, and a bellows-actuated latch at the lower end. Only the latch release system and the safety rod absorber element will be described in detail.
Figure 2: Alternate shutdown system

The pneumatic scram release mechanism* (PSRM) shown in Fig. 3 consists of a FERMI-type safety rod latch which is actuated by an adjacent bellows-spring combination connected by a fill/blowdown tube to redundant, normally open, solenoid valves above the vessel head. The latch helium supply is continuously connected to make up small leakages over a long period. Higher leak rates are detected by a loss of pressure and would ultimately cause the device to failsafe by inserting the safety rod. To scram the safety rod, the power to the solenoid valves is cut, causing them to open. The gas is expelled allowing the latch cam to descend under the action of the actuator spring (supplemented by the bellows spring force), its own weight, and a small component of the safety rod weight. The cylindrical latch cam has two sets of camming surfaces bearing on the latch

*U. S. Patent No. 3733251 (May 15, 1973)
fingers. The lower set bearing on the finger tips normally controls the motion of the fingers and uses a cam angle determined by the safety requirement that the latch should release even if the spring fails. The other set of camming surfaces bearing on the inside of the fingers serve as a positive, conjugate backup to the lower set.

The bellows are fabricated of Inconel 718 and the scram assist spring is Inconel X750. The body and structural parts are Type 304 stainless steel. Haynes Stellite 6H is applied to latch fingers and cam wear surfaces and on other moving parts, Inconel 718 was used where it would be sliding on Type 304 SS.

The absorber element** consists of 18 stainless steel clad, boron carbide absorber pins connected to a central structural column by pairs of hinged spider arms to allow parallelogram

**Patent applied for
motion. The central column has a lifting head and a spring loaded collar at the upper end and a dash pot ram at the lower end. When the lifting head is held by the scram latch, the spring is in a compressed state, thus providing an additional insertion force when the rod is released. The dash pot serves to absorb most of the kinetic energy of the rod at the end of its travel.

Two methods of attaching the spider arms are used. The arms carrying pins adjacent to the central column are attached in a spherical annulus T-slot, where the T-head proportions are such as to prevent jamming with clearances to allow for irradiation swelling. (See Fig. 4.) The center of rotation is thus on the vertical centerline of the absorber element so that the hexagonal packing geometry of the central column, the six adjacent pins and six of the peripheral pins, all of which have the same diameter, is satisfied. The remaining six pins are supported on clevis mounted spider arms which have pivots at a convenient radius from the column center line, and freely nest into the remaining space. It will be appreciated that this 18-pin configuration allows the pins to contract to their smallest possible hexagonal envelope. If more pins were required, then the ideal geometry could be preserved by putting T-slotted arms in planes axially offset from the first pair.

**PNEUMATIC LATCH TEST EXPERIENCE**

A test program was initiated in 1971 in order to determine the reliability of the pneumatic latch mechanism. Tests were performed both in air and in a high temperature sodium environment. The latch mechanism performed flawlessly during more than 2,000 cycles of operation, which is well over the number of scrams expected during the lifetime of the reactor plant. Post-test inspection showed little wear in the mechanisms and indicated a capability of operation of well beyond the 2,000 cycles tested.

The test latch was designed to be identical in every significant respect with the latch proposed for the reactor design. This had been sized to meet the spatial and loading requirements and used a bellows actuator conservatively de-

![Diagram](image)

**Figure 4:** Absorber rod flexible joint
signed to provide the estimated latch holding force with as low a gas pressure as the space would allow.

An approximate calculation made to predict the performance showed that the loss of bellows pressure would be delayed by the time taken for a rarefaction wave to traverse the blowdown tube at the velocity of sound. The subsequent latch cam motion was obtained by superposing the bellows force from the gas dynamics calculation upon the spring/mass system. It was concluded from the calculation that helium should be used as the working fluid in order to be sure of release within the design values of 100 ms.

A special latch test rig was built to investigate the latch performance in air and in sodium and to determine its reliability. Of primary interest was the durability of the welded, Inconel 718 bellows under conditions of impulsive pressure, mechanical loadings, and thermal shock, especially in the presence of sodium at 1100 F. Secondary objectives of the test program included establishing the ability to operate with some angular misalignment between the safety rod and the latch mechanism, to operate reliably after a long term dwell in high temperature sodium, and to determine operational characteristics of the PSRM under various pressures, temperatures, and spring force conditions.

The test assembly, shown in Fig. 5, was designed for preliminary testing in air before being mounted in a sodium filled test vessel. The vessel was connected to a clean-up loop which monitored the oxygen level in the sodium with a plugging meter and maintained the purity at less than 10 ppm oxygen by means of a cold trap. Heating controls maintained steady temperatures in the range 400 - 1100 F.

Figure 5 shows the guide tube, containing the latch at its lower end, inserted with representative clearance into a subassembly duct containing a simulated safety rod. The duct is remotely inclinable to give up to 3.5 degree angular offset and has provision for radial offset also. The PSRM has several alternative blowdown tube sizes and uses fairly fast (40 ms to fully open), commercial solenoid valves of the pilot-piston operated type. Thermocouples measure the bellows and sodium temperatures and pressure transducers are placed at each end of the blowdown tube. A displacement transducer is connected to the latch cam by a long, light rod which also can be used to transmit force from an adjustment spring outside the test vessel to modify the spring force on the cam. This rod is not present in the reactor PSRM design. During air testing a displacement transducer is also attached to the safety rod. Recordings are made with a light beam oscillograph operating with a chart speed of 2 meters/second.

The sample recording reproduced in Fig. 6 illustrates the sequence of events from the instant of solenoid de-energization. The 6 ms delay between the onset of pressure drop at the actuator and at the valve is the wave transit time at sonic velocity in the blowdown pipe.

It was found that pressure curves could be correlated for different gases and starting pressures by adjusting the time
Figure 5: Pneumatic latch mechanism test assembly

1389
Figure 6: Typical oscillograph recording

scale according to the velocity of sound and the pressure scale according to the ratio of the starting pressures. The release point shown is defined by the cam movement to open the latch fingers to just release the safety rod handling head during a relatively slow blowdown test. The release time was defined as the time from solenoid de-energization for the cam to descend to the release point.

The PSRM was operated with no failures to scram or involuntary scrams for nearly 2000 cycles, of which nearly 1600 cycles were conducted in sodium. Under standard conditions (1100 F sodium, 125 psig gas pressure), the release times were consistently in the range of 59 - 64 ms throughout 1200 cycles. Even under non-standard conditions (Table I), the average release times varied very little. A 10 ms initial deterioration in release time was found after a 30-day hold test but this is not regarded as significant compared with the total rod insertion time required. The reason for the small change in release time over the large range of temperature (400 to 1100 F) was evident from the pressure and displacement recordings. The loss in total spring force as temperature increased, due to a reduction in the elastic moduli, was largely compensated for by more rapid blowdown, due to an increase in the gas sonic velocity with temperature. The loss in spring force is clearly shown by the reduction in holding pressure. The slower release due to greater excesses of bellows gas pressure over the minimum holding pressure is also expected. Increasing the angular offset increases the minimum holding pressure, and consequently, reduces the margin of the actual pressure above it. This is clearly a safe trend since it shows that a large offset could cause release.

The temperature of the upper actuator bellows was found to fall by approximately 30 F during blowdown. This should
have no effect on the life of the approximately 0.01 inch thick bellows material. Following the tests, and while still operating consistently well, the pneumatic latch was dismantled, cleaned, and inspected. The bellows were found to be sound and only slight polishing was evident in the rubbing parts.

CONCLUSIONS

The pneumatic scram release mechanism and flexible safety rod assembly incorporated in the FERMI type control rod drive forms a practical alternate reactor shutdown system when combined with a partner shutdown system of the modified EBR-II type described above. The pneumatic latch has been shown by experiment to be highly reliable and has a satisfactory actuation speed (less than 65 ms). The absorber element is capable of inserting itself after release without external assistance inside a duct having much greater than anticipated distortion. The modified EBR-II shutdown system incorporates only proven components. Thus, the proposed combined LMFBR safety system is now developed to the point where only final proof testing is required.

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<th>1100</th>
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<tr>
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<td>72</td>
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<tr>
<td>Test pressure (psig)</td>
<td>Release time (ms)</td>
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<td>--</td>
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<tr>
<td>145</td>
<td>62</td>
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MISALIGNMENT TESTS AT 1100 F, 125 psig

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REFERENCES


THE ALTERNATE SHUTDOWN SYSTEM DEVELOPMENT PROGRAM

by

Ernest R. McKeehan
Bruce M. Shawver, D. Engr.

ABSTRACT

An alternate shutdown system development program is described. The system being developed is for application to the Liquid Metal Fast Breeder Reactor (LMFBR) and serves as a backup to the primary shutdown system. The system is discussed, and component development tests are outlined.

During the past two years a project has been underway at General Electric’s Advanced Technology Department (Breeder Reactor Operation) to develop an alternate shutdown system for application to the Liquid Metal Fast Breeder Reactor (LMFBR). The objective of this Alternate Shutdown System (ALSS) development program is to develop a highly reliable, independent, and diverse shutdown system so that gross damage to the reactor will be precluded even in the highly unlikely event that the Primary Shutdown System fails to scram. The primary goal is to demonstrate a system which has sufficiently high reliability that failure to scram can be eliminated as a design basis for the LMFBR. This project is sponsored by the USAEC and is focused on developing a system for incorporation in the Clinch River Breeder Reactor Plant.

To date, the program has included the development of a set of design criteria and the application of these criteria to a series of candidate concepts. A concept has been selected for development and is shown schematically in Figure 1. The principle of operation involves holding a control rod (which has a hexagonal array of 19 pins with enriched B,C as the absorber material) above the reactor core region against a downward hydraulic force that is developed by ducting the high pressure reactor coolant (sodium) to...
the top of a piston at the lower end of the control element. The region below the piston is vented to the low pressure plenum of the reactor. A latch at the lower end of the drive shaft supports the control element via a coupling head at the top of the control element. Upon deactivation of the latch, the coupling head is released, and the hydraulic and gravity forces rapidly insert the control element into the core region. A damper at the upper end of the control element slows the element during the final few inches of element travel. An important feature of this system is that, during scram, no translation of the control element or drive shaft is required across the horizontal plane between the reactor head and channel. This ensures scram capability in spite of relative radial motion between the head and the channels such as might occur during an earthquake.

A key point in the philosophy that has guided this Alternate Shutdown System development program is the inclusion of a considerable degree of diversity as a means of guarding against common mode failures within the shutdown system (i.e., failures in a mode common to both the primary and alternate portions of the shutdown system). The ALSS is being designed to provide a diverse and independent system that is capable, by itself, of shutting down the reactor in spite of any hypothetical failure of the primary system. The shutdown system should be designed so that failure of similar components through common mode events does not affect both the primary and secondary portions of the shutdown system. Therefore, the philosophy of diversity is important because it is crucial that any failure of the primary system caused by a failure of components in any of the primary system drives not be duplicated in nor propagated to the alternate system. Consequently, the ALSS must have operating and design characteristics different from the primary system. The ALSS must be not only diverse from but also independent from the primary system and not depend upon receiving scram commands from the primary system but rather be able to operate independently based upon an independent sensor and logic system. This independence is necessary if the alternate system is to be effective in guarding against events affecting the primary system, because some such events might be postulated to affect the alternate system through common interfaces if independence were lacking.

The primary motivation for including the ALSS in the reactor shutdown system is the desire to provide extremely high reliability for the reactor. To ensure that this reliability will be achieved, a thorough reliability study is being conducted. The objective of this study is to evaluate the inherent reliability of the ALSS and to confirm that the Primary and Alternate Shutdown Systems provide adequate diversity to prevent the occurrence of common mode failures. The approach to be employed in the study is:

1. To perform failure mode and effect analyses (FMEA) on both the Primary and Alternate Shutdown Systems.
2. To establish reliability and availability goals for the ALSS.
3. To predict system reliability and availability for the ALSS and compare the predictions to the goals.
4. To provide input to and analyze data from the test programs.
5. To evaluate the functional diversity of the components and sensors used by the two systems.

The failure modes and effects analysis will be prepared for both the Primary and Alternate Shutdown Systems to investigate the effects of component failures on overall system performance. The procedure will include listing all of the components of the system and the identifiable failure modes of each component. For each failure mode and failure mechanisms that could initiate that mode will be listed and studied. The effect on the system of each component failure mode will be identified and classified according to its impact on system performance.
Not only must reliability goals be established and met, but also availability goals for the ALSS must be determined. For the LMFBR to be a viable source of electric power, the plant must have a competitively high availability. Any alternate system included in the plant must not adversely affect availability, e.g., through spurious scrams. The development of the secondary control rod system will encompass the evaluations of the shutdown system's reliability and availability requirements with particular emphasis on evaluations of the alternate system.

The results of the failure modes and effects analysis will be used to evaluate the degree to which the system being developed meets the established requirements. This process is intended to eliminate deficiencies which might exist in the alternate system. If weaknesses are exposed, they will be corrected, and the reliability analysis will thereby benefit the overall system performance.

In a similar manner, the reliability analysis will aid in guiding the test program and in evaluating the data resulting from the tests. The FMEA will identify those components or design features that are critical and that would benefit greatest from design verification tests. Furthermore, through identification of causal events for the failure mode, the reliability analysis will focus attention on the test parameters and conditions that should be highlighted. The analysis will also form a framework on which to base the evaluation of the test data. Because these data can be used to establish failure rate statistics, they will aid in the overall quantitative reliability evaluation for the complete system that is to be installed in the plant.

Common mode failures are difficult to predict because the established statistical techniques that deal effectively with random failures are not applicable to analysis of common modes of failure. Furthermore, well developed failure rate data and interpretative methods that are available do not lend themselves to analysis of common mode failures. Because of this difficulty in dealing with common failure modes statistically, and because of the import of the consequences of occurrence of such failures, elimination of the possibility of these failures greatly enhances the confidence that is warranted in a system. Employing functional and equipment diversity is a powerful ally in eliminating common mode failures from consideration. By focusing on the design details of not only the alternate system but also upon those of the primary system, the degree of diversity between the two systems can be evaluated. This evaluation will be useful in identifying potentially common failure modes. As such failure modes are identified, they can be eliminated, or at the very least, safeguards can be instituted to guard against deleterious effects. This reliability study thus serves as a foundation that guides the program for achieving the primary objective of developing a highly reliable shutdown system.

The ALSS serves in a role that complements the capabilities of the primary system. The strengths of each system are different and dovetail in a way that enhances the overall capability of the shutdown system. The number of complementary areas is too large for all of them to be listed here. However, as an example one might be mentioned: scram under full reactor flow. Because the primary system control rod is inserted against the pressure force on it resulting from the drop in pressure from the inlet to the outlet of the control assembly channel, scram of that system tends to be slightly slower during full reactor flow conditions than during reduced flow conditions. Conversely, the ALSS control rod is driven into the core region by the available pressure (which is greatest, of course, during full reactor flow) and so reacts fastest during full reactor flow. Predicted insertion rates for the ALSS control rod under different reactor conditions will be shown graphically later.
In addition to the central effort of designing the system, the development program has branched into three major thrusts: supportive analytical efforts, component development testing, and full scale prototype testing.

The analytical efforts to date consist primarily of structural evaluation (e.g., irradiation and thermally induced channel distortion, buckling resistance during down-driving of the drive shaft, etc.); reliability analysis (e.g., failure modes and effects analysis, common mode failure analysis, etc.), and dynamic response modeling of the control rod during scram. This latter activity has involved the development of a digital computer code to model the reactor coolant flow through the control assembly during steady state and during scram. This model allows predictions of the scram performance (e.g., acceleration, velocity, and position vs. time) of the control rod. A sample of the program's output is shown in Figure 2 for scram from two initial conditions: nominal reactor operation at full flow and the worst case design basis transient (viz. loss of offsite power with 20% flow). The insertion times shown in Figure 2 are well within the required response for the reactor shutdown system. The dynamic model is being updated as the design progresses. Its validity will be checked during full scale prototype testing. Preliminary verification will occur during the upcoming component development tests.

![Figure 2. Displacement vs Scram Time](image)

Subsequent to the selection of the concept, several critical components were identified for development and testing. The three items first selected for early testing were the:

1. Damper Performance
2. Latch Operation
3. Flow Characteristics of the Control Element

Tests are now underway for the damper. The damper is shown schematically in Figure 3. Near the end of the scram stroke, the tapered dashram section enters the down stop tube and slows the element. Then, the damper spider contacts the down stop. As the control element continues to move, the damper is...
compressed and dissipates the kinetic energy of the element, thereby bringing it to a gentle stop. Although the action of the tapered dashram is relatively obvious, the action of the plate damper is not so apparent and, therefore, warrants some discussion.

As the damper is compressed, the plates, which are held apart initially by conical springs, are brought together and force the fluid out radially and then axially through the annular gap between the periphery of the plates and the bore of the housing. During the first half of the damper stroke, while the velocity is still relatively high, the primary damping effect comes from the piston action associated with accelerating the fluid through the annular gap. As the stroke progresses and the plate-to-plate gap decreases, the effect of squeezing fluid from the interstices between plates becomes more pronounced. Thus, as the damping stroke progresses, the type of damping shifts from one type to another. This allows the damper to have the desired characteristics throughout the entire stroke.

This damper was first tested in a simplified bench test rig using a hydraulic cylinder to apply load to the damper. After varying the critical parameters and dimensions to determine optimum performance, a promising configuration was selected for testing in the rig shown in Figure 4. This rig simulates the geometry expected in the reactor. For convenience, water rather than sodium is used as the working fluid. A latch holds the simulated control element in position and releases it for scram. The element is driven down by...
hydraulic forces that can be controlled in this test by varying the water pressure. The damper will be tested over a wide range of conditions to determine its characteristics and performance capabilities. Fabrication of the rig is now complete and testing is being initiated.

![Image of damper test rig]

**FIGURE 4. DAMPER TEST RIG**

The latch that supports the control rod above the core region is shown in Figure 1 is also scheduled to undergo extensive testing. Details of the latch are shown in Figure 5. The latch is of the collet type and is held in the closed position by a tension member connected to a pneumatic cylinder. Rapid venting of the cylinder through large capacity, quick-acting valves allows release of the tensile force and, hence, release of the coupling head from the latch. To ensure that high friction coefficients and self-welding in high temperature sodium will not interfere with proper operation, the latch is undergoing strenuous testing in a prototypic environment to verify that the design performs as expected. The rig is designed to provide prototypic loading conditions and allows release and reset cycling of the latch. Selected material combinations are being tested under conditions of rapid cycling (to accumulate information on wear characteristics) and long-term holds (to determine self-welding tendencies under these relatively high contact stress conditions). Inconel 718 in contact with itself and type 316SS coated with
chromium carbide in a nichrome binder in contact with Inconel 718 are the candidate material combinations that are being tested for the highly stressed (Hertz contact stresses on the order of 60,000 psi) contact surfaces of the latch. Because of the degree of uncertainty involved in the friction coefficient to be encountered in the reactor, the latch has been designed to accommodate a wide range of friction coefficients (0.2-2.0). Static friction coefficients are expected never to exceed 1.0, and dynamic friction coefficients are expected never to exceed 0.8.

Tests on other components are scheduled for the future and are now in the planning and early design stages. Because the control element flow test is still in the preliminary design stages, a discussion of it would be premature. Coolant flow through the control assembly (including the control element, channel, and inlet orifices, etc.) and position indication devices are among those items planned for testing. The testing is intended to verify the predicted performance and will pave the way for prototype fabrication and testing that is scheduled to begin within a year. A series of 20 prototypes is planned for testing. The reason for such a large number is that statistical information is being sought so that the high degree of reliability that is required can be demonstrated through actual experience.

The intent of this development program is that alternate shutdown system be developed for application to the LMFBR so that the ultimate in safety and reliability can be achieved for the next generation of nuclear power plants. A diverse and independent shutdown system is undergoing successful development to meet that need.

FIGURE 5. ALTERNATE SHUTDOWN SYSTEM LATCH
GCFR's SOLUTION TO RADIATION AND THERMAL DISTORTION

H. C. Hopkins and H. J. Snyder, Jr.

General Atomic Company

ABSTRACT

The design features of the Gas-Cooled Fast Breeder Reactor (GCFR) that allow accommodation of radiation and thermal distortion without resorting to a complex core-restraint mechanism are discussed. A comparison of radiation swelling and geometric parameters for the GCFR and LMFBR are presented to illustrate why a restraint system is unnecessary. The analysis of GCFR core behavior is described briefly.

INTRODUCTION

In the Gas-Cooled Fast Breeder Reactor (GCFR), as in LMFBRs, the phenomena of neutron-induced swelling and irradiation-enhanced creep, as well as differential thermal expansion, must be considered in the reactor design. These cause two basic forms of fuel-assembly distortion to occur in the high flux region: (1) bowing of the fuel-assembly duct due to differential swelling and (2) dilation of the duct due to swelling and creep. Initial work on distortion has been focused on the unique and advantageous features of the GCFR design—the reactor support system and the negligible neutron absorption by helium—which allows for the accommodation of these distortions by the provision of adequate space between the assemblies.

The pertinent features that are unique to the GCFR are illustrated in Figs. 1 and 2 and are summarized below:

1. The single grid plate is mounted above the reactor to which each assembly is rigidly attached.

2. The assemblies are clamped solely at their "cold" ends, which permits freedom of movement of the lower ends, prevents strain-energy buildup, and results in negative reactivity as the fuel assemblies bend outward due to swelling.

3. The coolant flow is downward through the core, which maintains the grid plate at the inlet gas temperature (=600°F).

4. The control rods enter the core at the cold clamped end of the fuel assembly through the grid plate so that control-rod alignment is not affected by core distortion.

5. The fission-gas plenum in each fuel rod is eliminated by removal of gaseous fission products through a pressure equalization system to an absorption system that is separate from the primary coolant.
Fig. 1 Cutaway view of GCFR PCRV

Fig. 2 Cutaway perspective of GCFR core
6. The helium coolant has negligible neutron absorption characteristics, thereby minimizing the effects of increasing the core void volume and permitting a conservative clearance (0.250 in.) between adjacent fuel assemblies.

An additional feature is the strong radial orientation of the neutron flux gradients that are not greatly perturbed by the low-worth (<$1$) control rods and the resultant dominant radial orientation of distortion.

The GCFR demonstration plant reactor consists of 118 fuel assemblies and 127 radial blanket assemblies, a cross-section of which is shown in Fig. 3. The grid plate is a Type 304 stainless-steel forging and the fuel-assembly ducts and fuel-rod cladding are 20% cold-worked Type 316 stainless steel. The burnup goal is 100,000 MWD/tonne maximum and the peak neutron fluence in the core is $2.0 \times 10^{23} \text{nvt (E > 0.1 MeV)}$.

**COMPARISON OF GCFR AND LMFBR**

One of the principal features of the GCFR is the absence of a core-restraint system similar to those incorporated in the FFTF and in the LMFBR demonstration plant. These restraint systems add a considerable degree of complexity and uncertainty to the core design. To illustrate why a restraint system can be avoided in the GCFR, a quantitative comparison of swelling and geometric parameters has been made of the two types of demonstration plants.

**Fuel-assembly Bowing**

The bowing of fuel assemblies is caused by differential swelling across the duct that results from the radial neutron fluence gradient and temperature gradient. This causes a curvature to occur in the ducts in the active core region and a deflection at the free end of the assembly that is proportional to the length of the assembly from the core midplane to the free end. The GCFR does not require a fission-gas plenum space in the fuel rods because the fuel is vented and thus the unfueled length is less than one-half that in an LMFBR fuel assembly. A comparison of the two assemblies is shown in Fig. 4. The LMFBR fuel assembly extends 99 in. from the midplane to the free end, whereas the GCFR assembly extends 49 in. Thus, for equal differential swelling across the assemblies, the deflection of the GCFR would be one-half that of the LMFBR.

The fluence and temperature in the assemblies and their gradients determine the differential swelling and the resulting radius of curvature of the ducts. A comparison of these parameters and their relative deformation results are tabulated in Table I. The bowing comparison is made for an assembly on the edge of the core where the neutron flux gradients are largest and result in the greatest bowing. In the GCFR, the average fluence is 20% less and the average temperature of the duct is 50°F less than in the LMFBR. Although the temperature gradients in both reactors are about the same, the fluence gradient is about one-third that in the LMFBR. This results in a swelling gradient that is 33% of that for the LMFBR. When coupled with the geometric difference between systems, the resultant deflection at the end of the GCFR assemblies is about 17% of that in an LMFBR.

The control rods enter the core from the clamped end of the fuel assembly, so control-rod drive alignment is not affected by the total assembly distortion, as would occur in the LMFBR where the control drive enters from the free end. The deflection at the outlet end of the GCFR core, which is the furthermost insertion point of the control rod, is less than 7% of that at the entry point of the LMFBR drive. This small deflection in the GCFR is entirely accommodated internal to the GCFR control-rod guide tube by articulation at the control-rod...
Fig. 3 Reactor cross section

Fig. 4 Comparative GCFR and LMFBR fuel assembly profiles
### Table I
COMPARATIVE SWELLING PARAMETERS FOR LMFBR AND GCFR DEMONSTRATION PLANTS

<table>
<thead>
<tr>
<th></th>
<th>LMFBR</th>
<th>GCFR</th>
</tr>
</thead>
<tbody>
<tr>
<td>Center Assembly, Core Midplane</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Maximum flux, $nv$</td>
<td>$4.1 \times 10^{15}$</td>
<td>$3.1 \times 10^{15}$</td>
</tr>
<tr>
<td>Residence time, hr</td>
<td>$1.4 \times 10^{6}$</td>
<td>$1.8 \times 10^{4}$</td>
</tr>
<tr>
<td>Maximum fluence, nvt</td>
<td>$2.1 \times 10^{23}$</td>
<td>$2.0 \times 10^{23}$</td>
</tr>
<tr>
<td>Temperature, °F</td>
<td>850</td>
<td>800</td>
</tr>
<tr>
<td>Edge Assembly, Core Midplane</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Average flux, $nv$</td>
<td>$2.4 \times 10^{15}$</td>
<td>$1.5 \times 10^{15}$</td>
</tr>
<tr>
<td>Flux gradient, $nv$/in.</td>
<td>$2.9 \times 10^{14}$</td>
<td>$0.75 \times 10^{14}$</td>
</tr>
<tr>
<td>Average fluence, nvt</td>
<td>$1.2 \times 10^{23}$</td>
<td>$1.0 \times 10^{23}$</td>
</tr>
<tr>
<td>Fluence gradient, nvt/in.</td>
<td>$1.5 \times 10^{22}$</td>
<td>$0.5 \times 10^{22}$</td>
</tr>
<tr>
<td>Temperature gradient, °F/in.</td>
<td>27.7</td>
<td>26.0</td>
</tr>
<tr>
<td>Bending Arm Length</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Assembly, in.</td>
<td>99</td>
<td>49</td>
</tr>
<tr>
<td>Control, in.</td>
<td>99</td>
<td>20</td>
</tr>
<tr>
<td>Comparative Swelling and Distortion</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Relative swelling</td>
<td>1.0</td>
<td>0.38</td>
</tr>
<tr>
<td>Relative swelling gradient</td>
<td>1.0</td>
<td>0.33</td>
</tr>
<tr>
<td>Relative unconstrained assembly distortion length effect</td>
<td>1.0</td>
<td>0.5</td>
</tr>
<tr>
<td>Relative unconstrained assembly total deformation</td>
<td>1.0</td>
<td>0.17</td>
</tr>
</tbody>
</table>

The smaller, more compact control rod is the result of GCFR reactivity-worth requirements that are less than one-half of those for the LMFBR.

An analysis of the fuel-assembly deflection resulting from bowing that was based on an earlier, more conservative swelling correlation (3) indicates a very small interaction of fuel assemblies at only a few points on the outer edge of the core. This interaction occurs at the bottom of the fuel assembly and can be eliminated by a slight reduction in the size of the outlet nozzle. The fuel-assembly pitch could also be increased slightly with only a small effect on the nuclear performance since the increased void fraction would be filled with nonmoderating gas.

The distortion resulting from irradiation-induced swelling and creep occurs at a very slow rate over the lifetime of the core and results in a small overall decrease in reactivity. If the fuel assemblies do not interact or bear against one another, this distortion cannot result in any inadvertent increase in reactivity. If the fuel assemblies do not interact or bear against one another, this distortion cannot result in any inadvertent increase in reactivity and thus represents the safest possible concept. It is also a concept that can be analyzed with a high degree of certainty.

The fuel-assembly bowing distortion also affects the design of the remote fuel-handling mechanism. In the GCFR, the transparent coolant provides an opportunity to visually observe the fuel-element nozzle locations to determine whether the assembly distortion is greater than anticipated. The remote fuel-handling equipment is aligned at a predetermined location for the assembly and then it automatically adjusts to the exact element withdrawal position by sensing the position of the assembly outlet end. The removal or insertion of an assembly
Fuel-assembly Dilation

A dilation or bulging of the fuel assembly will occur around the midplane of the core due to irradiation-induced swelling and creep. The swelling depends on the fluence and temperature at the duct. Table I lists values for a center assembly at the core midplane. For comparable fuel burnup, the fluences are about the same for the LMFBR and the GCFR. However, the temperature is lower in the GCFR because of the larger temperature drop across the core for the same outlet temperature as the LMFBR. The mean duct temperature is 50°C less in the GCFR at the position of maximum fluence. The swelling is a strong function of the temperature of the duct. The resultant swelling dilation at the assembly midplane is 38% of that in the LMFBR.

The creep results from a pressure difference across the duct at about the position of maximum fluence. In the GCFR, this occurs at a location below the middle of the fuel element so that the pressure drop across the duct wall is less than one-half the total pressure drop through the fuel element, whereas in the LMFBR, it occurs at a location that results in about two-thirds of the total fuel-element pressure drop across the duct wall. However, the total LMFBR pressure drop is three times that in the GCFR, so the net effect is that the pressure causing dilation in the GCFR is one-fourth that of the LMFBR. The width of the flat section of the duct wall is larger in the GCFR and the thickness is slightly smaller so that the resultant dilation is of the same magnitude in both systems. However, the GCFR can accommodate more room for dilation because of the small nuclear effect caused by the increased spacing between the fuel elements.

ANALYSIS OF DISTORTION

The distortion of GCFR fuel assemblies was analyzed and displayed using General Atomic computer programs. The three distortion phenomena analyzed are radiation swelling, assembly thermal expansion, and grid-plate distortion caused by thermal expansion and elastic-force displacement. Radiation swelling produces a volume increase in the assembly walls that is dependent on the total fast neutron fluence (E > 0.1 MeV) and the wall temperature during irradiation. Since fluence and wall temperature vary both radially and axially within the reactor, the assembly walls undergo varying amounts of radiation-induced expansion and resultant deformation. Also, the wall temperature of the hexagonal elements varies both circumferentially and axially, depending on the power and coolant-flow distribution. The local temperature of the assembly wall is equated to the adjacent coolant temperature. The resultant thermal expansion produces local deformations. Each assembly is treated individually and its distorted position is computed without regard to interference. The objective of the analysis is to prescribe conditions that eliminate interference. Interference between elements is indicated where assembly positions overlap.

The analysis that was made of the accommodation of radiation swelling and thermal distortion in the GCFR core(1,2) represents a preliminary, conservative estimate in which an early swelling correlation(3) that did not include irradiation-enhanced creep was used. Since more recent swelling correlations(4) predict a smaller degree of swelling for a given fluence-temperature combination (perhaps only one-half that predicted by the earlier correlation), the calculations presented are quite conservative. Analysis has been initiated to check and verify the GCFR predictions using the more-detailed structural code CRASIB,(5) which includes irradiation-enhanced creep. Typical assembly distortion is calculated to be 25% less using CRASIB.
The fuel assemblies are rigidly connected to the grid plate, and they move as the grid plate expands and deflects slightly. Initially, the grid plate is at room temperature and essentially flat. After the assemblies are loaded into the grid plate, the weight of the assemblies causes a slight deflection of the grid plate and a resultant displacement of each assembly. When the reactor is producing power, the grid plate is heated to the inlet helium temperature and it expands radially; all assemblies are thus displaced outward from the center assembly by this thermal expansion of the grid plate. Additional deflection is caused by the operating pressure difference across the grid plate. The operating clearance is greater than room-temperature clearance and thus there is additional room for distortion.

Local fast neutron fluences ($E > 0.1$ MeV) and relative power are required input information. These are a product of a physics code that lists fast neutron fluxes and a relative power at the reactor midplane for each corner of each hexagonal fuel assembly. Sets of values are prepared for ten time points in the fuel cycle, which correspond to partial refueling times at the end of a cycle or at the beginning of a cycle. The current physics code produces a data tape with 12,000 values that contains the input for the swelling calculation; this tape is read by a subroutine of the distortion programs. To obtain fast fluence values, the fluxes at the beginning and end of a fuel cycle are averaged and multiplied by the time of full-power operation. The history of each assembly being calculated is required input and is composed of the residence time, units of rotation at each refueling time, and the pairs of interchanged assemblies. At a particular refueling time, the total fluence is the sum of the fluences back to zero residence times. The fast flux and power gradients are strongly radially oriented, as shown in Fig. 5, where power is transformed into temperature; this causes a regular pattern of distortion that allows a systematic reduction of distortion during the operating life of an assembly by rotation and shifting its position in the grid plate. When assemblies are rotated or shifted, the fluence values are taken from the appropriate location. The radiation distortion is also rotated because it is dependent on past radiation swelling history.

The raw output of the computer program is a three-dimensional matrix of positional data for each assembly that locate points on the corners of the hexagonal box along the vertical axis. To interpret this information, pictorial representations are essential, and these are obtained directly from a computer-controlled plotter.

Since the clearance between assemblies of an unirradiated cold core is about 0.250 in. and the distance between opposite sides of the hexagon is 6.7 in., it is desirable to transform the positional data so that distortion is magnified and the change in clearance between assemblies can be observed.

Each R-Z plot represents eight fuel assemblies starting at the center of the core and moving outward along a radius. The radius chosen is perpendicular to the walls of the assemblies in a row and is oriented as shown in Fig. 3. Along this radius, the locations of a point on the inner and the outer wall and the location of a point on the centerline of each assembly are computed at 46 axial locations. A true scale plot of these data would yield a series of nearly vertical lines, with three lines per assembly. The clearances or interferences are so small on this scale that they would result in insufficient resolution. In the R-Z plots, the radial distortion is greatly magnified by transforming the location data for the assemblies. To magnify the distortion, the assembly dimensions at room temperature are subtracted from the location values, leaving the radial location of the lines shifted by the room-temperature clearance between assemblies. The transformed R-Z plot at room temperature before irradiation is shown in Fig. 6. The eight fuel assemblies are represented by single lines that are separated by the room-temperature clearance of 0.250 in. Each
Fig. 5  Direction of largest temperature decrease with rods inserted

Fig. 6  R-Z plot - 70°F and unirradiated
of these lines is the superposition of three lines representing the inner wall, the centerline, and the outer wall of that assembly. As thermal expansion and radiation swelling occur, the assembly walls distort and the plot will show three lines for each assembly. In Fig. 7, at initial startup and operating temperature, only the thermal expansion effects are shown. Figure 8 is a typical plot of the combined results of distortion, where the center bulging from radiation swelling and the greater bending of the assemblies are evident. An overlapping of the sets of lines would indicate interference between adjacent assemblies.

In the R-θ plots, the dimension of interest is the distance between adjacent assemblies and the distortion data are transformed to magnify the scale of these distances. Because the reactor is rotationally symmetrical for each 120° sector, one 120° sector will show all possible assembly clearances. A true scale R-θ plot of the reactor for a 120° sector is shown in Fig. 9. Here, the clearances and interferences are poorly resolved because they are small compared to the dimensions of the hexagons representing the fuel and blanket assemblies. Again a transformation was used that resolved the clearances between assemblies.

The resultant representations were called gap plots since the corner distance between hexagons equals the gap between assemblies. A gap plot with no distortion is shown in Fig. 10. The distance between adjacent corners is a scaled 0.250 in. in every case. Figures 9 and 10 are superficially similar since there are six sides for a hexagonal fuel assembly and there are six gap end-points associated with each assembly; however, the orientation of the corners is rotated 30° since the gaps are located between assembly sides.

For this discussion, only some representative results of the interference design condition are presented. In this case, one-third of the core is replaced during each refueling cycle and the fuel assemblies remaining in the core are rotated 180° during the refueling shutdown. Figure 11 is a typical R-Z gap plot for a time point in the fuel cycle, and Fig. 12 is the same time point for a R-θ gap plot.

Similar plots were prepared for all fuel-cycle time points to determine if the design criteria of negligible contact between assemblies was met.

For the 180° rotation at refueling conditions, the following observations were made:

1. Only six pairs of core assemblies are observed to interfere—two at 900 days and four at 1125 days. The maximum interference is about 0.1 in. There are no interferences at the core-blanket interface.

2. The blanket-blanket assembly interferences are few in number. The worst conditions exist at 1125 days in the region of element 75.

CONCLUSIONS

The combination of a 0.250-in. clearance between fuel assemblies and rotating the core fuel assemblies at each refueling provides a satisfactory design solution to the estimated radiation swelling that is of concern to all fast-reactor designers. This solution does not depend on mechanically restraining the assemblies, which would greatly increase the complexity of the reactor design.

It is further concluded that the GCFR has a number of unique design features that minimize the effect of fuel assembly distortion and makes possible a core design that does not require a core restraint system for the anticipated magnitude of irradiation-induced swelling and creep. These features are summarized in Table II.
Fig. 7 R-Z plot – full power and unirradiated

Fig. 8 Typical R-Z plot – full power during equilibrium fuel cycle
Fig. 9 True scale R-Θ gap plot - full power during equilibrium fuel cycle

NOTE - 70°F (POWER = 0)

Fig. 10 R-Θ gap plot - 70°F and unirradiated
Fig. 11 Typical R-Z plot during equilibrium fuel cycle after 450 days

Fig. 12 Typical R-Θ gap plot during equilibrium fuel cycle after 450 days
Table II
SUMMARY OF UNIQUE GCFR CHARACTERISTICS LEADING TO ELIMINATION OF CORE RESTRAINT

<table>
<thead>
<tr>
<th>Characteristic</th>
<th>Description</th>
</tr>
</thead>
<tbody>
<tr>
<td>Gas coolant</td>
<td>Allows more space to accommodate dilation and bending distortion, eliminating assembly interaction</td>
</tr>
<tr>
<td>Top fuel support</td>
<td>Fixed control-rod alignment</td>
</tr>
<tr>
<td>Top entering control</td>
<td>Eliminates seismic shear plane</td>
</tr>
<tr>
<td>Vented fuel, shorter fuel elements</td>
<td>Reduced assembly distortion; Reduced control channel distortion</td>
</tr>
<tr>
<td></td>
<td>Eliminates axial reactivity shift; Unconstrained axial growth; Provides negative bowing reactivity coefficient; Eliminates need for lateral support</td>
</tr>
<tr>
<td>Clamped fuel elements</td>
<td></td>
</tr>
<tr>
<td>High internal conversion, low control-row worth</td>
<td>Uniform radial flux gradient with minimum control-rod effect results in uniform radial bending Relaxes lower assembly position accuracy; Allows visual evaluation of core distortion and responsive action</td>
</tr>
<tr>
<td>Visual refueling</td>
<td></td>
</tr>
<tr>
<td>Lower neutron flux gradients</td>
<td>Reduces assembly distortion</td>
</tr>
<tr>
<td>Lower core midplane temperature</td>
<td>Reduces assembly distortion</td>
</tr>
</tbody>
</table>

The verification of the GCFR unconstrained core concept will be based on fast test reactor irradiation information from the United States and on actual demonstration-plant performance in Europe. Analytical work currently in progress will provide a basis for design and this will be verified by parallel analysis to be performed by Argonne National Laboratory.

Single-ended fuel-element support inherently provides a negative power coefficient of reactivity and also generally avoids the potential positive reactivity contributions associated with lateral core-restraint-system forces and with strain forces in constrained ducts.

REFERENCES

SESSION 14
INSTRUMENTATION AND PROBABILISTIC METHODS
Chairman: K. Wirtz (KfK)
Single Subassembly Fault Detection System -
Functional Requirements and Shut-off Criteria

K. Gast, D. Smidt

Institut für Reaktorentwicklung
Kernforschungszentrum Karlsruhe

Abstract

Considering potential modes of single subassembly fault propagation, the critical chain of events is identified. Several stages of fault development are distinguished and the relevant phenomena are discussed with regard to their potential of causing damage, their time scale of progression and methods of detection. Preliminary functional requirements are derived for the subassembly fault detection system. Finally, a set of alarm and shut-off criteria is presented as an example.

1. Introduction

A considerable portion of our LMFBR safety work is devoted to the problem of single subassembly failure propagation \[1, 2, 3\]. By propagation we mean the spreading of a fault which develops in a single subassembly at or near steady state reactor power to surrounding assemblies. The importance of this problem stems from the fact that local cooling disturbances and fuel pin failures are relatively likely to occur, that they are not readily detected by a safety system which only monitors the gross behaviour of the core and that - in the postulated case of uninterrupted progression of the fault - dangerous modes of propagation have to be envisaged which conceivably may either initiate a whole core accident or render the shut off system inoperable. As potential accident initiating events a fuel coolant interaction (FCI) and sudden release of large amounts of fission gas from the plenum region of a single subassembly have to be considered.

As of today's knowledge, the single subassembly incident cannot a priori be regarded as hypothetical. Further investigations are needed of the more important phenomena and special safety measures must be taken in order to demonstrate a sufficiently low probability of untolerable damage to the core. Towards this goal, several types of activities are pursued in parallel:

1. Investigation of the most important phenomena involved such as local blockages, pin failure mechanisms and pin to pin failure propagation, local and bulk boiling in a subassembly, fuel coolant interaction.
2. Development of special instruments for early detection of the fault and of a suitable data processing and trip generating system.

3. Some potentially dangerous modes of fault propagation must be designed out by providing inherent safety features.

4. Determination of the maximum possible damage resulting from undetected failure propagation, i.e. from a large coherent FCI in any one subassembly.

One cannot expect to obtain sufficient proof of safety by only following one or two of these lines. On the other hand, the progress obtained in each of these areas is complementary with regard to the required evidence of safety. This paper deals mainly with items 1 and 2; the activities under item 4 are discussed in a separate paper [17].

2. Initiating Faults and Incident Chains

There are three types of initiating events:

a) Local blockages developing in the fuel rod bundle

b) Subassembly inlet blockages

c) Spontaneous rupture of cladding tubes in the gas plenum region of the bundle.

a) **Local blockages** may lead to progressive failure of fuel pins or to local sodium boiling or both and thereby become self-propagating until cooling of the subassembly is essentially interrupted by bulk boiling and sodium ejection. Unless the reactor is shut down at this stage, this would be followed by rapid overheating and melting of fuel and, possibly, by an explosive vaporization of sodium which eventually would get in contact with molten fuel and clad. More and better evidence is needed to prove, that a FCI could neither directly (by reactivity effects) nor indirectly (by distortion of control rod guide tubes) lead to a whole core accident. For the time being we, therefore, have to rely heavily on local fault detection.

b) **Subassembly inlet blockages** may be caused by an object blocking the inlet duct or by particulate matter accumulating in the lower portion of the subassembly. Such blockages can be reliably detected by monitoring subassembly flow and/or outlet temperature before the integrity of the fuel bundle is threatened, provided that they originate over a time period which is slow compared with the time needed for detection and shut off. Sudden and nearly complete blockages - occupying more than about 95% of the inlet flow area as needed for bulk boiling - are conceivable only at the subassembly foot, e.g. by a piece of metal foil. In this case, bulk boiling could hardly be prevented by subassembly instrumentation. This led us to the following requirement:

The inlet ducts of fuel and breeder elements must be designed such that a near complete blockage by a single foreign object is impossible. This may be achieved by provid-
ing several inlet openings protected from simultaneous blockage by their geometrical arrangement.

Thereby and with the safety measures necessary to cope with the faults of type a., which will be discussed later, inlet blockages become an uncritical path of the fault tree.

b) Clad rupture in plenum region
The third chain of events may proceed in a very short time and, consequently, there is no way to interrupt it by an active system: The pressure transient associated with a spontaneous clad rupture in the gas plenum region of a fuel pin could cause a large number of cladding tubes to burst almost simultaneously. If the fission gas plenum is below the core, a major fraction of the gas would expand into the inlet plenum and hence be swept back into the core through neighbouring subassemblies, thereby inserting sodium void reactivity. This problem can readily be solved by a simple modification of the fuel pin design which was suggested in [2] and will be used for the SNR reactor: Inside the clad there is a concentric tube consisting of one or more pieces which are closed at both ends by plugs containing small size holes. In case of a clad rupture, the fission gas can only escape slowly because of the choking effect of these holes. This will strongly reduce the probability of other clad failures. However, even if simultaneous rupture of all pins in a single subassembly would occur, no dangerous reactivity excursion could result because of the retarded gas ejection.

3. The Critical Chain of Events and Means for Interrupting it

From the reasons discussed in the previous chapter, the incident chain starting from local blockages was concluded to constitute the critical path of the propagation fault tree. This chain, therefore, determines the functional requirements for the safety system.

It is useful to distinguish the following stages of development of the local incident:

1. Local (single phase) cooling disturbances and pin failures in the fuel rod bundle
2. Local sodium boiling
3. Bulk sodium boiling in the subassembly
4. Gross melting of clad and fuel, FCI.

It is postulated, that at least two instrumental safety barriers must be erected for timely and reliable interruption of the incident chain. These barriers may become effective either in different stages of the incident or in the same stage. Evidently, a detection device can only be claimed an effective barrier, if the event to be detected will necessarily or most likely occur, as the fault progresses.

Therefore, in order to determine appropriate safety barriers and to derive their functional requirements, the following questions must be answered:
Which phenomena will necessarily or most likely occur in the respective stage?

By what methods can these phenomena be detected and what is the reliability of detection (e.g. signal to noise ratio)?

Could any phenomena possibly occur at this stage, which are dangerous with regard to the propagation accident itself?

How fast can this stage be passed and, hence, how much time is available for detection and counteraction?

3.1. Stage 1: Local Cooling Disturbances

Local disturbances within the fuel rod bundle may originate from changes in subchannel geometry (e.g. thermal bowing of rods, clad inflation, local failure of spacer grids), from accumulation of particulate matter or from failure of fuel pins.

These mechanisms may individually or commonly lead to a growth of the blockage. Changes in subchannel geometry cause only a small temperature raise and are, therefore, considered uncritical. Solid impurities can be essentially eliminated as a cause of local blockages by appropriate design and operation. In particular, a screen provided in the subassembly foot would make sure that impurities can only lead to a harmless blockage of type b (chapter 2).

Therefore, progressive failure of fuel pins is considered as the prevailing mechanism for the postulated growth of blockages. Pin failure propagation may be caused by ejection of fission gas or by escaping fuel.

3.1.1. Blockage Growth by Pin to Pin Propagation

It is beyond the scope of this paper to discuss in any detail the large area of pin failure propagation. Our present assessment of this very complex problem is as follows. Today's experience is insufficient to completely rule out the possibility of pin failure propagation. Since a very effective and reliable safety barrier, i.e. delayed neutron detection, is available against slow propagation (timescale minutes and longer), one only needs to worry about fast propagation, however improbable it may be.

From our own and other authors investigations we came to believe, that neither fission gas ejection nor "washing out" of solid fuel particles can cause rapid failure propagation. Rather, one must concentrate on the possibility of ejection of molten fuel which might either directly affect neighbouring pins, lead to rapid growth of the blockage or generate pressure pulses due to a local FCI.

Central fuel melting could be caused by

- the elevated coolant temperature in the disturbed region

- inflation of the clad resulting in high thermal gap resistance

- chemical attack of fuel by sodium following a leak in the clad
producing a reaction product of lower density and thermal conductivity [4].

- higher enrichment of individual pellets.

Although investigations performed at Argonne [5] indicate, that molten fuel will relocate axially within the pin, rupture of the clad cannot be excluded, particularly at considerable burnup. The mechanical effects of a local FCI are believed to be very weak because of the small amount of molten fuel involved and because of the incoherence of the interaction. Furthermore, the effect of a local FCI on the bulk coolant flow should be very similar to that of local boiling, as Fauske [5] and Cronenberg have shown using a bubble dynamics model similar to the one described in [6]; this implies, that hydrodynamic instability of flow is not to be expected from a local FCI.

Pending the results of in-pile experiments on pin failure mechanisms at elevated clad temperatures and pin to pin propagation [7, 8], we presently take the position that a local FCI would not be dangerous in itself with regard to the criteria given in chapter 1. This will be further substantiated by our program dealing with the mechanical consequences of a FCI [9].

As regards blockage growth, we are somewhat less optimistic: It seems doubtful that in-pile experiments on pin to pin propagation in the near future will yield sufficient evidence to rule out fast pin failure propagation completely. For the time being, therefore, a strategy is used in dealing with the single subassembly incident which can do without the knowledge of the time scale of blockage growth.

3.1.2. Blockage and Clad Failure Detection

To determine the detectability of blockages by monitoring subassembly flow or temperature, simulation experiments were made using full size models of the SNR subassembly with planar blockages [10]. The investigation of coherent, planar blockages appears conservative and also reasonable because grid spacers will be used in SNR. The relative reduction in subassembly coolant flow caused by a blockage of certain size is different for subassemblies at different radial positions in the core because of the effect of the inlet nozzle. In Fig. 1, the reduction of subassembly flow is plotted versus blockage size (% of total flow area in the bundle) for the highest rated and for the lowest rated subassembly. It should be noted, that deviations from the nominal flow which would be acceptable as trip levels from the operational point of view correspond to blockages larger than 70 %. This statement also holds for the associated increase in subassembly outlet temperature.

Temperature noise analysis is also being investigated as a possible method for blockage detection [11]; here, difficulties arise from the need for fast response thermocouples and from data processing. Other effects which may be used for detection are low level pressure transients and flow oscillations caused by violent pin failures. For the time being, however, priority is given to methods of clad failure detection. The main objectives for BCD systems are high sensitivity, short response time and - from the operational point of view - the ability of localizing the defect.
fuel element. Our experience so far indicates, that the delayed neutron technique is very promising with regard to the first objective, while further effort is needed to meet the latter two. With these targets in mind, an alternate or additive burst can detection-system (BCD) was developed at Karlsruhe [2], which should be capable of detecting and localizing a single pin failure of the progressive type within a second. This system is based on the separation and accumulation of fission gas bubbles at the subassembly outlet by means of a vortex generator; an electrically heated thermocouple gives a fast and large signal if surrounded by gas instead of sodium. Typical signals obtained in model experiments with water and a few cm$^3$ of injected gas are shown in Fig. 2.

The probe type flow sensor developed by Interatom may also be capable of detecting small amounts of fission gas [12], in particular if combined with the vortex generator mentioned above.

3.2. Stage 2: Local Sodium Boiling

3.2.1. Starting Conditions

If a local blockage continues to grow, it may eventually lead to local boiling of the coolant. Local boiling could either initiate a rather fast growth of the fault by causing rapid pin failures due to dryout and melting or degenerate to bulk boiling due to vapor production and hydrodynamic instability of flow. An extensive program is underway at Karlsruhe with the objective to demonstrate that local boiling would produce neither one of these mechanisms. This program also includes the development and testing of methods for boiling detection.

A series of single phase experiments was performed in a water rig with 1:1 scale heated models of the SNR subassemblies and planar blockages of varying size and location, in order to determine the temperature field in the wake behind the blockage as the starting condition for local boiling [13]. From the experimental results the corresponding temperature profiles in the SNR subassembly were calculated. In Fig. 1, the difference between the peak coolant temperature in the wake from the saturation temperature taken as 1000 °C is plotted versus the size of a central blockage. As can be seen from this graph, local boiling is unlikely to occur in the wake even if as much as 50% of the bundle cross-section is completely blocked. On the other hand, blockages of this size can hardly be detected by monitoring outlet temperature or flow, as was mentioned before.

This approach, i.e. using planar blockages for single phase and also for boiling experiments, deliberately neglects the possibility of boiling to occur within a blockage. Here our line of arguments is as follows: Blockages will preferably be formed at spacer grids and are unlikely to be complete. Furthermore, the effects of local boiling will be the more pronounced, the more subchannels are already blocked at the onset of boiling. In order to be conservative, one should, therefore, concentrate on investigating large blockages up to and above 70% of the bundle cross-section, this being about the threshold of detection by monitoring the mixed mean outlet temperature. As a blockage of this size builds up, heat will almost exclusively be removed from it by residual flow through the blockage, and the boiling temperature will likely first be reached in the wake behind it. If one further assumes the
bundle geometry in the wake not to be grossly disturbed, the problem can be reduced to investigating local boiling in "clean" geometry.

3.2.2. Nature and Detectability of Local Boiling

A theoretical model of local boiling has been developed [2, 6] which assumes that incipient boiling superheat will be in excess of about 20 °C and which predicts the growth and collapse history of individual vapor bubbles in a given temperature field. Typical bubble histories for blockage of 18 % and 56 % and for different values of superheat are shown in Fig. 3. The main results of this analysis may be summarized as follows:

- Depending on superheat and blockage sizes, individual vapor bubbles typically reach size up to a few inches and recondense completely within about 40 msec.
- During the bubble life, the temperature of the "hot" fuel rods within the bubble drops well below the value just prior to boiling due to film evaporation.
- Local boiling will neither cause rapid pin failures due to dryout and melting nor hydrodynamic instability of flow.
- Local boiling will cause bulk coolant flow pulsations of several 10 % with ramps of the order 10/sec, which may be detected by a probe type flowmeter.

Sodium boiling experiments performed in a test section of simple geometry [14] yielded good agreement as far as bubble dynamics is concerned. More representative experiments with full size partially blocked heater bundles are in preparation [15]. This program is expected to substantiate our preliminary conclusions concerning the second stage of fault development, namely:

- Local boiling does not cause a rapid growth of the fault even in case of large size blockages which can be detected by monitoring subassembly outlet temperature; in particular, transition to gross boiling due to hydrodynamic instability is not to be expected for such blockage sizes.
- As in the first stage, the prevailing mechanism of the postulated progression of the fault is blockage growth by pin failure propagation; this process - if at all existing - would be considerably retarded, however, because of the enhanced heat removal under local boiling conditions.
- There appears to be a good chance to detect local boiling by monitoring outlet flow. Moderate hope is placed in boiling noise and pressure pulse detection, pending the results of special in-pile and out-of-pile experiments currently underway.

3.3. Stage 3: Bulk Sodium Boiling

If the subassembly coolant flow is reduced below about 35 % of its nominal value either due to continued growth of a local blockage or to an inlet blockage, the incident enters into the bulk boiling phase, which may be characterized as follows: (A detailed discussion of our work on bulk boiling is presented in another paper of this conference, see ref. 15).
At the onset of boiling, sodium is rapidly ejected from the upper portion of the subassembly. Depending on superheat, the ejection velocity may be somewhat larger than the nominal coolant velocity (up to 10 m/sec. Subsequently, the liquid column oscillates in the top part of the element with varying amplitudes of several 10 cm and frequencies around 5 Hz, while the heated section of the bundle is essentially emptied from sodium flowing as multiple slugs; the time scale of this process is determined by the size and location of the blockage.

Complete reentry of sodium is prevented by high velocity vapor flow which originates from the liquid film remaining on or partially being renewed at the fuel rod surfaces. Consequently, dryout and clad melting must be expected within less than 1 sec, followed by gross melting of fuel after a few seconds (3 to 6 sec, dependent on subassembly rating). The boiling phenomena themselves are not dangerous with regard to fault propagation to the surrounding element, as was demonstrated by the BEVUS experiment [16]. Even at very high "enforced" superheat, boiling and condensation pressure pulses caused no measurable deformation of the affected subassembly duct, let alone its neighbours.

It should be pointed out, that prediction of the bulk boiling behavior of partially blocked subassemblies is still debatable. The uncertainty in extrapolating the results of former experiments stems mainly from inadequate modelling of a subassembly. Further experiments with rod bundles and a better hydraulic simulation of a fuel element are, therefore, being prepared [15]; besides testing of methods for boiling detection, this program is also aimed at demonstrating that effective cooling in the boiling regime can be maintained over more than a few seconds, especially in the low power breeder elements.

Several methods of bulk boiling detection are being investigated. Although the thermal hydraulic behavior of the coolant above the subassembly exit is difficult to predict, we believe that bulk boiling can be readily detected by the flow sensing unit foreseen for the SNR reactor. In order to eliminate the masking effect of neighbour assemblies on the signal, it is considered to let the unit protrude from the instrument plate into the coolant exit ducts. Other methods of boiling detection, including acoustic and reactivity noise as well as pressure pulse detection, are being pursued with comparable effort.

3.4. Stage 4: Gross Melting of Clad and Fuel, FCI

In the phase of fault development following bulk boiling and dry-out of the fuel rod bundle, the following phenomena must be considered:

a) Fuel slumping
b) Penetration of hot fuel into neighbour subassemblies
c) Mechanical and nuclear effects of a fuel-coolant interaction in the incident subassembly.

da) The reactivity ramp associated with fuel slumping in a single subassembly is easily coped with by the conventional safety system and, therefore, needs no further consideration. On the other hand, the slumping reactivity may be quite welcome as an
effect which can be used for timely detection of the subassembly fault. However, only limited credit can be given to the reactivity meter today, because coherent slumping cannot be relied upon (compensating positive and negative reactivity effects) and because a FCI could possibly occur prior to slumping.

b) Penetration of neighbouring subassembly ducts could cause cooling disturbances and, possibly, bulk boiling in these subassemblies. Even in the most improbable case of simultaneous initiation of bulk boiling in the six surrounding fuel elements, prompt criticality via the void effect can be excluded. Furthermore it is argued, that an equal, contemporaneous and undetected development of secondary faults in surrounding subassemblies leading to, e.g. simultaneous bulk boiling and fuel coolant interactions in these assemblies may be ruled out on probability reasons. Penetration of subassembly ducts is, therefore, considered as an uncritical mode of fault propagation.

c) We, therefore, concentrate on the molten fuel-coolant interaction as the only mechanism in the course of a single subassembly fault which could potentially cause a whole core accident. Despite a great number of theoretical and experimental investigations performed in the past, the FCI is only vaguely understood. In particular, no reliable prediction can be made today about the mechanical damage caused by a hypothetical, coherent FCI. Therefore, a rather straightforward experimental and theoretical program is being performed with the objective to verify that the maximum deformations of the surrounding core structure resulting from a FCI in any one subassembly would be tolerable from the safety point of view.

This program, which is the subject of another paper given at this conference [17], includes underwater explosion tests with full scale models of the SNR core and FCI-experiments with up to 5 kg of molten UO₂ and sodium under representative geometrical conditions. The main objective of the core model tests is to establish an upper bound of pressure time histories which result in acceptable deformations of the structure, both regarding reactivity effects and jamming of control rods. The FCI experiments are intended to yield the evidence, that this upper bound would not be exceeded in a single subassembly spert incident.

It should be pointed out that the safety evidence expected from this program will likely not be sufficient to eliminate the need for a single subassembly fault detection system. However, depending on the quality of evidence obtained, the requirements for the preventive system may be substantially relaxed.

4. Preliminary Requirements for the Protection System

4.1. General Requirements

In the preceding chapters, we have outlined our assessment of the single subassembly incident, considering the present state of the art in fault analysis and instrumentation development. On this basis, some general requirements for the protection system may be formulated: (It is presupposed that - regarding a special instrumentation system for fault detection - every appropriate measure is taken to prevent local faults. Such measures should include design features for inherent safety, e.g. at the subassembly
inlet, as well as a high level quality assurance program, e. g. preoperational purification of the sodium systems, stringent control of fuel enrichment etc.

- Cooling disturbances and faults in any individual subassembly must be detected reliably and in time, such that the reactor would be shut off before a considerable amount of fuel in this subassembly becomes molten.

- Timely emergency shut down of the reactor must be guaranteed by redundant and possibly diverse fault detection and signal processing. There should be at least two instrumental safety barriers which - preferably - become effective in different stages or in the same stage of fault development. Prerequisites of an effective safety barrier are a) the phenomenon which is to be detected will necessarily or most likely occur in the considered chain of events b) detection is sufficiently reliable and c) counteraction is initiated automatically and in time if the signal exceeds a certain limit. Depending on availability and sensitivity of the respective sensors, reliability of detection may be guaranteed by diversified or by simplex fault detection.

- The conception of the Multi Input Safety System and the shut-off criteria may be based on the supposition that all events occurring in the course of a single subassembly fault prior to and including bulk boiling in this subassembly do not themselves constitute an immediate danger for the reactor from the safety point of view. Regardless of this supposition, use should be made of possible means to detect local faults in earlier stages, even if the respective signals are not fit for direct scram (e. g. flow meter noise) or if the time lag between event and recording does not permit, at present, to guarantee coverage of the entire range of possible time scales of fault propagation (e. g. detection of delayed neutrons).

- The reliability of fault detection and production of a trip signal should be comparable to the reliability of the shut-off system such that the probability of failure to scram is not considerably increased by the probability of a fault escaping detection up to and including bulk boiling.

4.2. Approach to Solution

These general requirements can be met in several different ways. Obviously, by providing a greater number of instrumental barriers in series, the probability of a fault escaping detection may be reduced or the availability requirement for an individual barrier may be eased. On the other hand, the complexity of the system thereby increases, particularly because signal patterns and, hence, relevant shut off criteria are different for different stages of fault development. These aspects and the prospect of frequent sensor failures would favour a computerized system with extensive software capable of modifying and adjusting the shut-off criteria to the respective history and state of the individual subassembly, thereby being rather intensive to sensor failures. However, such a system is beyond the present state of the art and would probably not be accepted in the licensing process. Therefore, a compromise must be found considering the partly controversial aspects of reliability of detection, simplicity of the detection system, frequency of sensor failures and avoidance of unnecessary scrams. Since the problems involved are mainly due to the large number of

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sensors in case of individual subassembly instrumentation, strong efforts are made with the long term objective to eliminate the need for individual safety instrumentation by providing reliable integral methods of detection. For the time being, however, we feel that at least one of the required safety barriers must be provided by subassembly instruments connected to the secondary shut off system. The following rationale may be applied as basis of the overall detection system:

- According to the previous statements, automatic shut off must be guaranteed within 3 sec after the onset of bulk boiling in any one subassembly at the latest.

- Because of the present uncertainty of boiling detection, the fault must be reliably detected prior to bulk boiling by monitoring subassembly flow and/or outlet temperature.

- Early pin failure detection is another important line of defense. The delayed neutron detection may be employed as the main constituent of this barrier, the sensors being connected to the primary shut off system.

- Pending the results of current investigations concerning pin to pin failure propagation, blockage growth, transition from local to bulk boiling and the mechanical effects of fuel coolant interactions, the aforementioned barriers may be supported by monitoring subassembly flow variations. (Tests performed with the probe type flowmeters developed by Interatom indicate their potential for detecting fission gas bubbles as well as local and bulk boiling [12].)

A computerized Multi Input Safety System (MISS) will likely have to be used for processing the large number of data from the subassembly instrumentation and for the generation of trip signals [18]. Presently, a system is being favoured which basically consists of 3 multiplexer/analog-digital converter units for digital signal branching and 3 synchronized communicating process computers working in a 2 out of 3 logic. Preliminary functional specifications call for cyclic monitoring within 1.5 sec of 205 subassemblies instrument units consisting of 3 thermocouples and, possibly, one flowmeter each.

4.3. Tentative Alarm- and Shut-off Criteria

In the following, a set of tentative alarm and shut off criteria is presented as an example of such criteria which would, to the authors' opinion, meet the general requirements mentioned before. These criteria and, in particular, the numerical values of the trip levels, are intended as a basis for discussion and will likely be subject to changes. Alarm or shut off would be initiated by MISS if one of the respective criteria is met for any one subassembly.

A) Alarm

Criterion A1: \[ T \geq T_i + 1.05 \cdot \Delta T_{nom} + 4 \, ^{\circ}\text{C} \]

(2 out of 3 thermocouples)
T = subassembly outlet temperature

$T_i$ = reactor inlet temperature

$\Delta T_{\text{nom}}$ = nominal temperature rise

$\Delta T_{\text{nom}}$ is computed for the individual subassembly or, alternatively, for groups of subassemblies according to position in the core, burnup and reactor power. Actions following alarm from any one subassembly may be:

- acoustic and optical warning signal in the control room
- analog display of the signals coming from the "suspicious" element and of its temperature history in the control room via the process computer external to MISS
- scram by the operator in case of coincident signals from integral detection devices, e. g. delayed neutron monitors, acoustic noise, which are still below the automatic trip levels established for these systems.

B) Set-back of reactor power

Set-back in case of alarm does not seem to be advisable for several reasons. This point needs further consideration, however.

C) Automatic Shutt-off

Criterion C1: $T \geq T_i + 1.10 \Delta T_{\text{nom}} + 8 \, ^\circ C$

Criterion C2: $T \geq 650 \, ^\circ C$

(2 out of 3 thermocouples)

Criterion C2 is mainly intended to serve as a simple back up with regard to erroneous computation of $\Delta T_{\text{nom}}$ in criterion C1. However, C1 and C2 do not constitute two independent, effective safety barriers in the sense of the general requirements listed before.

The second barrier will probably be formed by connecting the delayed neutron monitors to the conventional safety system. Trip levels for these devices will have to be established following evaluation of current and future in-pile experiments with failed fuel pins. As mentioned before, this line of defense may require support by other devices which are - as an optimum - capable of detecting fast blockages and rapid pin failures as well as local boiling. These functions could principally be performed by an electromagnetic flowmeter. Therefore, a third criterion was tentatively formulated as a back up:

Criterion C3 (optional)

Variation of flow signal $Q_N$

Flowmeter defect $Q_D$

Subassembly outlet temperature $T \geq T_i + 1.05 \Delta T_{\text{nom}} + 4 \, ^\circ C$
This criterion takes advantages of the important capability of the flowmeter to detect rapidly developing faults and, on the other hand, attempts to prevent unnecessary scrams upon failure of the flow sensor, for which redundancy can hardly be provided from technical reasons. The logic AND-connection with relatively low temperature trip levels is, therefore, considered reasonable both with regard to safety and economic requirements. Alternatively and depending on the amount of safety credit to be taken from the flowmeter, the criterion C3 may be suspended for those subassemblies containing defect flowmeters and the reactor allowed to operate with up to a certain number of failed flow sensors for a certain time.

Concluding, it is reiterated that the general requirement of reliable and timely reactor shut-off can be met in several different ways. It is our believe, that by intelligent use of today's technology the safety needs imposed by the problem of single subassembly faults can be satisfied without undue burden to the economy of LMFBR's.

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Fig. 1. Typical flow reduction in a partially blocked (planar, central blockage) subassembly and distance from saturation temperature in the wake.

Fig. 2. Temperature signals from simulated clad failures.

Fig. 3. Bubble growth and collapse for local boiling at the upper end of the core. Temperature profile B.
ROLE AND DEVELOPMENT OF LMFBR PROTECTION SYSTEMS

by

J. B. van Erp and H. K. Fauske
Argonne National Laboratory, Argonne, Illinois 60439

Abstract

It is recommended that a balanced approach be followed with respect to reliance on (1) reactor safety shutdown systems (plant protection systems), and (2) engineered safeguards, for protection of public health and safety: Reactor safety shutdown systems, as well as engineered safeguards (containment systems, blast shields, post-accident-heat-removal systems, etc.) of high reliability and proven performance should be provided for fast breeder reactors.

Inherent safety for hypothetical core-disassembly events, obtained predominantly by means of passive engineered safeguards, should remain a long-range goal in the development of fast breeder reactors. For the short-term future, upper values of energy releases and destructive work, relative to hypothetical core disassembly accidents assumed as design bases for engineered safeguards, should be established on the basis of what is, for a particular plant, expected to be a reasonably provable high limit in the light of the present state of knowledge and the expected results from on-going and near-future research, rather than on the basis of what could possibly be calculated as an extreme upper limit by following accident scenarios, obtained by compounding numerous improbable and very conservative assumptions. Such an approach will require the development of plant protection systems of high reliability and proven performance, having, at all levels, adequate characteristics of redundancy, diversity, and insensitivity to common-mode failures.

The present state of knowledge for LMFBRs does not allow to exclude the possibility that a single whole-subassembly accident may develop into a whole-core accident. Even though the probability for such an event is small, it is not small enough by itself, so that single-subassembly protection is necessary to further reduce the probability of the occurrence of whole-subassembly accidents.

A rational approach to the design of complex reactor safety shutdown systems (plant protection systems), protecting against (1) whole-core accidents initiated by high-frequency events (anticipated transients), (2) whole-core accidents initiated by low-frequency events, (3) local-core accidents (including single-subassembly accidents), and (4) loss of capital investment, would include distinction of various subsystems having different reliabilities, gradated in accordance with the frequency and severity of the accidents to be protected against. Such an approach would permit application of novel techniques (including computer technology) in parts of the protection system, in order to handle the high number of measuring channels, necessary for local-core protection; it would require revision and adaptation of current US standards for plant protection systems.

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1. Introduction

In principle, two extreme positions are possible in the safety approach followed for protection of the general public and the environment against release of radioactive materials (fission products or other) from the cores of nuclear power reactors, namely:

(1) complete reliance on the reactor safety shutdown system(s), for prevention, or minimization of core damage in case of a malfunction, and

(2) heavy reliance on engineered safeguard systems, such as containment systems, blast shields, core-catchers, post-accident-heat-removal (PAHR) systems, etc., for prevention of dispersal of radioactive materials into the environment in case of a core disassembly accident.

The first approach requires availability of reactor safety shutdown systems (or plant protection systems, PPS) with high reliability* and proven performance,* whereas the second approach necessitates detailed knowledge of the physical processes leading to, and prevailing during and subsequent to a core disassembly event, as well as availability of engineered safeguard systems having high reliability and proven performance. Both approaches, if carried to an extreme, have obvious shortcomings as will be discussed in more detail in a later section of this paper.

An important distinction that needs to be made with respect to the foregoing is that between active and passive systems, the primary difference being whether or not the system in question depends on outside energy sources for its performance. As examples may serve the following: Blast-shields, containment systems not relying on isolation valves, core-catchers, and post-accident heat removal (PAHR) systems not relying on pumps, etc., are considered passive systems in the above sense, whereas reactor safety shutdown systems, emergency core cooling systems (ECCS) requiring pump operation, etc., fall in the class of active systems. A further important difference distinguishing passive from active systems is that, whereas both types of systems may be subject to performance failures (i.e., the system does not perform as intended in its design), it is primarily the active systems that are subject to reliability failures (i.e., the system does not start to function when called upon). Table 1 gives a brief summary of the above. It thus follows from the foregoing that reliability requirements are not only of importance for reactor safety shutdown systems, but also for those engineered safeguards which depend for their performance on active components. Many systems have both passive and active characteristics, such as, e.g., certain PAHR systems, capable of operation at a reduced heat removal rate by means of natural circulation of the coolant. Clearly, wherever possible, passive systems are to be preferred over active systems, since the former are not subject to reliability failures.

2. Long-Range versus Short-Term Objectives

There exists, no doubt, great attractiveness in nuclear reactor systems that are inherently safe, i.e., for which a stable end-condition is reached that is acceptable from the point of view of public health and safety for all types of postulated accident scenarios, without intervention by means of active components. Unfortunately, none of the nuclear power reactor types that have reached, up to now, commercial maturity have attained complete inherent safety in the above

* For a description of the meaning of the terms reliability and performance in the present context, we quote from Ref. 1, page 12: "It is self-evident that protective instruments must perform functions correctly and adequately. Not only must something happen when needed (reliability), but what happens must do the job (performance)."
sense. This is not to deny that most reactor systems have many inherent safety features (negative Doppler coefficient, negative void coefficient for the coolant/moderator in light water reactors, etc.); however, these features are, by themselves, not sufficient to guarantee an acceptable end-condition for the core without reliance on intervention by means of active systems, for all chains of events that can be postulated in the safety evaluation.* Still if nuclear fission reactors are to become an important part of a long-range solution to the world's urgent energy supply problems, then it certainly seems justified to continue a considerable effort in striving to approach the ideal situation of complete inherent safety, obtained predominantly by means of passive systems.

An important question that could be raised is whether any nuclear power reactor system can be developed to be completely inherently safe (in the sense described above), while still fulfilling the conditions of economic viability. It has been said facetiously that the safest nuclear reactor system is that which does not get built since it is not economically viable. Such a nuclear reactor system is, however, also not apt to make a very substantial contribution towards the solution of our urgent energy supply problems. This situation is not unique for the nuclear energy field. The basic fact is that all industrial activities carry some risk, however small. Demanding the impossible in risk reduction will render any industrial activity economically nonviable, and thus deprive society of its benefits. For every industrial activity the risks have to be carefully weighed against the societal benefits.3,4

For the liquid metal cooled fast breeder reactor (LMFBR), we find ourselves somewhat in the following position: On the one hand, its characteristics make it less desirable to rely on active systems for ultimate protection of public safety than for thermal reactors (in this connection should be named the fact that the fuel, under normal conditions, is not in the most reactive configuration); on the other hand, however, it is not excluded that a rigid licensing position, in which no credit is given for protective intervention by means of emergency safety shutdown systems (passive and/or active), may render the LMFBR economically nonviable, at least for some time to come. Without any credit for the safety shutdown system(s), the analysis of normally harmless incidents, such as, e.g., coolant flow coastdown, will inevitably terminate in core meltdown. And once the analysis is followed to the point of partial or total core meltdown, many different scenarios are possible, including second and subsequent criticality, none of which can, in the present state of knowledge, be easily disproven with a sufficiently high confidence level to be able to exclude them. These various scenarios will, of necessity, result in a wide spread in the outcome of the evaluation of the total energy that could be released before a stable end-condition is reached. It may also, for some time to come, be difficult to give an absolute maximum energy release, since it cannot be guaranteed that all possible scenarios have been considered. At some point, presumably one will have to make a best judgment in order to establish design requirements for the blast shields, the containment system, the PAHR system, etc.

It may well be that, in the present state of knowledge, the uncertainty in the sequence of events following a hypothetical LMFBR core meltdown and in the magnitude of the potentially possible energy release, could be equally large or larger than the uncertainty in the reliability and performance of well-designed and thoroughly-tested reactor shutdown systems. Furthermore, for the latter, the reliability can be improved by following available and well-established engineering methods, whereas for the former it may continue for some time to be difficult to come up with "hard" numbers as regards upper limits for the energy release of end-of-spectrum accidents, particularly for plants in the 1000-2000 MWe range. On the other hand, the reliability of a protection system has two main sources of

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* The recently issued new USAEC licensing position for light water reactors implies recognition that reliance is placed on the plant protection system.2
uncertainty, namely (1) common-mode failures (including, unidentified failure modes), and (2) human-induced failures. As to this latter source, the reliability of a protection system, consisting largely of active components, is not better than its operating and maintenance staff, an aspect that would gain particular importance if a large number of reactors were to be operated.

The impact on the overall plant capital investment for LMFBRs, of a rigid licensing position in which no credit is given for emergency safety shutdown, has up to now not been properly quantified, but may be expected to be considerable. Perhaps one of the more urgent studies to be performed should be aimed at developing information regarding the cost of passive systems (blast shields, containment system, PAHR system, etc.) versus total energy released for core disassembly accidents, with plant nominal power as parameter.

In view of the above, and without losing sight of the long-range goal of inherent safety as defined in the foregoing, it would seem that a balanced approach for the short-term future, covering the period of construction and operation of demonstration plants and the first generation of large-scale plants, should comprise the following elements:

(1) Development of highly reliable plant protection systems (passive as well as active) of proven performance, for protection against:
   
   (a) Whole-core accidents initiated by high-frequency events (anticipated transients; such as loss-of-flow accidents caused by pump coastdown, etc.),
   
   (b) Whole-core accidents initiated by low-frequency events (such as primary pipe rupture, passage of a large coherent gas bubble through the core, collapse of the core support structure, e.g., due to seismic events, etc.),
   
   (c) Local-core accidents, involving a single fuel subassembly or a limited number of subassemblies, and
   
   (d) Loss of capital investment.

(2) Establishment of upper values for the energy release and the destructive work, relative to hypothetical core-disassembly accidents assumed as design bases for engineered safeguards, on the basis of what is expected, for a particular plant, to be a probable and reasonably provable high limit in the light of the present state of knowledge and the expected results from on-going and near-future research, rather than on the basis of what could possibly be calculated as an extreme upper limit by following accident scenarios, obtained by compounding numerous improbable and highly conservative assumptions.

(3) Continued research at a considerable effort level (both analytical and experimental) aimed at gaining a better understanding of the physical processes leading to, and prevailing during and subsequent to, hypothetical core disassembly events.

(4) Development of engineered safeguard systems, of high reliability and proven performance, capable of

   (a) withstanding the energy release and the destructive work as defined under the above point (2), and
   
   (b) containing the core debris indefinitely in a stable end-condition, without release of radioactive material into the environment.

In order to be able to fulfill the conditions of high reliability and proven performance for LMFBR protection systems, considerable research and development is required. Unfortunately, one was confronted in the past with a vicious circle:
If, in the licensing procedures, the possibility of credit for emergency shut-
down of the reactor is excluded from the beginning, then no incentive exists to
make R&D funds available for the purpose of developing shutdown systems of high
reliability and proven performance; and if those latter systems are not available,
then clearly there is no justification to give credit to them in the licensing
procedures. This state of affairs, which for LMFBRs has prevailed for a number
of years, is inconsistent with that existing for thermal nuclear reactors, such
as, e.g., the light water reactors, the heavy water reactors, and the graphite
moderated gas-cooled reactors.5,6

Relying, in the study of hypothetical end-of-spectrum accidents, on safety
shutdown systems for protection of public health and safety, would permit to pro-
cceed with the construction of LMFBR demonstration plants and the first generation
of large-scale LMFBR commercial plants, in order to (1) obtain the necessary
operating experience, (2) evaluate the economic viability of the overall concept,
and (3) allow a normal evolutionary development process to take place: It is
the operating experience from such a first generation power plants which con-
stitutes an extremely important ingredient in the further development of safe
liquid-metal-cooled fast breeder reactors for the long-term future.

3. Protection Against Local-Core Accidents

While it is considered that the probability is low for a single subassembly
accident at full power to develop (via meltdown and molten fuel/steel-coolant
interaction) into a whole-core accident, it is also recognized that this possi-
bility cannot, in the present state of knowledge, be excluded with a sufficiently
high degree of confidence7-10 to allow it to be ignored. This state of affairs
may change as the results of on-going and near-future research becomes available,
and when more operating experience is gained. Figure 1 indicates how a whole-
subassembly accident could develop starting from three initiating processes,
namely, (1) pin failure, followed by pin-to-pin and blockage propagation, (2)
randomly distributed coolant subchannel flow obstructions, and (3) subassembly
inlet blockages.

Extensive analytical and experimental studies11-15 have shown that pin fail-
ures are unlikely to propagate to adjacent pins, and that pin-to-pin and blockage
propagation, if at all possible, are slow processes of low probability. Early
detection of the formation of a hypothetical extensive impervious local blockage
due to failure propagation, is possible by means of fuel element detection and
location (FEDAL) systems, monitoring fission products in the cover gas and the
reactor coolant; 15,16 for large blockages of this type, extending over 50% of the
subassembly cross-sectional flow area, detection is feasible also by means of
subassembly outlet instrumentation (flow and/or temperature sensors).12,16-18

Subchannel flow-obstructions, distributed randomly (both axially and rad-
ially) in the subassembly, have an extremely low probability to result in exten-
sive impervious local blockages, and will affect the overall subassembly flow,
making detection possible by means of subassembly outlet instrumentation well
before the occurrence of a severe power/flow mismatch.

Subassembly inlet blockages will also affect the overall subassembly flow,
allowing detection during their formation or during the startup procedures, prior
to a severe power/flow mismatch; furthermore, proper design of the subassembly
inlet region should make the probability of the occurrence of sudden subassembly
inlet blockages very low.

Even though the probability for the occurrence of a whole-subassembly acci-
dent is low, and notwithstanding the fact that a whole-subassembly accident is
not likely to develop into a whole-core accident, the overall probability is not
low enough in view of the possible severe consequences. Single-subassembly
Protection is thus required to further reduce to acceptable levels the probability for whole-subassembly accidents (probability \( < 10^{-6} \) per reactor year).

Protection channels, protecting against single-subassembly accidents, can be allowed to have lower reliability than protection channels, protecting against whole-core accidents initiated by anticipated transients,\(^{19}\) in view of the differences in frequency, probability, and severity between the former and the latter, and also since, as mentioned earlier, not all unprotected whole-subassembly accidents will develop into whole-core accidents. Furthermore, since single-subassembly malfunctions are expected to develop slowly in the initial stages, it appears strongly preferable to execute protective action in case of subassembly incidents by means of reactor power runback in order to keep severe thermal shocks to the system to a minimum, rather than by means of rapid emergency shutdown (scram) of reactor power.

As has been pointed out in a number of publications,\(^{20,21}\) the large number of signals, required for adequate single-subassembly protection, are best handled by means of computer systems. Such computer systems would enable to extract considerably more information from the sensors than could be possible in case of a protection system employing the usual bistables. Furthermore, it is expected that the use of computer systems would make it possible to keep the frequency of spurious interventions of the plant protection system within allowable limits, while still attaining the design reliability for protective purposes (e.g., by making a trade-off between frequency of spurious interventions and protective reliability).

An excellent review paper by R. D. Smith\(^{22}\) gives the world-wide status as of October 1972, concerning single-subassembly protection; Table 2 is reproduced from this paper.

Good candidate signals for single-subassembly protection that are being considered are derived from

1. subassembly outlet temperature sensors,
2. subassembly outlet flow sensors,
3. failed element detection and location (FEDAL) systems,
4. pressure pulse (acoustic) sensors,
5. anomalous reactivity detectors,
and possibly also
6. neutron noise analyzers

The above sensor types (1) and (2) would be required for each individual subassembly, unless a trade-off is made between the sensitivity of measurement and the required numbers of sensors.\(^{23}\) The sensor types (3) can either by installed so as to monitor each individual subassembly or they can be installed to monitor the entire core; for the former installation mode the response times can be considerably shorter than for the latter. The sensor types (4) and the sensing systems (5) and (6) are capable of providing whole-core information, while still being able to detect single-subassembly faults,\(^{17,18}\) under certain conditions. Figure 1 indicates at what stages of a postulated sequence of events, leading to a whole-subassembly meltdown, the various sensors and sensing systems are expected to provide reliable signals.

Subassembly outlet thermocouples (and possibly also flow sensors) are required for (1) plant start-up procedures, and (2) optimum plant operation, resulting in better economic performance; using signals from subassembly outlet instrumentation in the plant protection system would enable to decrease the probability for the occurrence of whole-subassembly accidents and would not necessarily constitute a high cost increment, if suitable computer technologies are applied.
FEDAL systems are expected to have a strong impact on the overall economic performance of LMFBRs, since they allow detection and location of subassembly malfunctions in the early stages, thus enabling replacement of faulty subassemblies before substantial contamination of the coolant, and well before the occurrence of a potential whole-subassembly power/flow mismatch. Signals derived from FEDAL systems should be considered for the plant protection system, in particular in connection with protection against local core accidents.

Anomalous reactivity detecting systems and pressure pulse (acoustic) sensors enable early diagnosis of core malfunctions, and should be considered for protection against single subassembly accidents.

Neutron noise analyzers should be investigated as to their potential as diagnostic tools for detection of core anomalies in LMFBRs, such as, e.g., flow-induced vibrations of core and vessel internals, etc.

4. Overall Protection Strategy

As mentioned in the foregoing, the tasks of the reactor safety shutdown system(s) are to provide protection against: (1) whole-core accidents, initiated by high-frequency events (anticipated transients), (2) whole-core accidents, initiated by low-frequency events, (3) local-core accidents (including single-subassembly accidents), and (4) loss of capital investment.

Clearly the above protective functions are not required to have equally high reliability since the frequency of the initiating events, and the potential consequences for the unprotected case are widely different. A rational approach to the design of the overall protection system would thus allow to distinguish various subsystems having different reliabilities, gradated in accordance with the frequency and severity of the accidents to be protected against; such an approach would probably require revision and adaptation of current U.S. standards for plant protection systems.

The design of complex protection systems, having to fulfill the tasks listed above under points (1) through (4), and having to meet the various requirements of reliability, diversity, and insensitivity to common-mode failures, etc., would make it desirable to follow a systems-engineering approach with the aim of obtaining an optimum solution in terms of the overall level of protection attained versus the magnitude of capital investment made.

In order to meet the stringent reliability requirements, associated with anticipated transients which potentially could lead to whole-core accidents (failure rate $< 10^{-7}$ per demand), it will probably be necessary to install two completely separate protection systems as a means for reducing common-mode failures.

Figure 2 gives a schematic representation of a possible strategy for the overall protection system design. Two separate systems are used, interconnected only at the actuator level. The principal characteristics of the first system are (1) small number of reactor safety shutdown (scram) channels ($< \sim 10$), (2) failure rate (unreliability) $< \sim 10^{-7}$ per demand, (3) strictly controlled access for all vital components (installation possibly to be in vaults) when the plant is in operation or not subcritical by a large margin. This latter requirement is aimed at protecting against human-induced failures, either inadvertently or intentionally (sabotage). All circuitry of the first system would be hard-wired. The second protection system would have the following main characteristics (1) possibly relatively large number of reactor safety shutdown channels, (2) reliability gradated, with failure rates between $10^{-3}$ and $10^{-5}$ per demand, (3) easy access of all components for testing, repair, and maintenance. Furthermore, this second protection system would, in parts or as a whole, be allowed to use computer technology.
In addition to the conventional design of protection systems (consisting of sensors, logic circuitry, and actuators, depending heavily on active components), it is desirable to consider introducing passive triggering devices (e.g., thermal or fissile fuses), which could be used in conjunction either with existing actuators or with separate passive neutron poison systems. Such latter systems would be completely passive, from the sensing to the actuation function, and could be made to activate at higher set-point values than the normal plant protection system(s), so as to avoid their being triggered, except in the case when all other systems have failed; furthermore they could be made completely inaccessible and tamper-free, so as to reduce to a minimum the possibility for human-induced errors.24

5. Recommended Development Areas

In the light of the information available at the moment, it appears that some recommendations can be made regarding development areas for protection systems meriting early attention:

A first necessity is the development of a well-defined overall strategy for protection of public health and safety, as well as of capital investment, against both whole-core accidents and localized core malfunctions. In order to be able to develop such a strategy, a large body of information is needed, covering, among others, the performance and the reliability of (1) sensing systems, (2) shutdown logic systems, (3) primary and backup safety-shutdown actuator systems, (4) surveillance and alarm systems. Trade-off studies need to be performed in which the increased costs for more instrumentation, a larger number of protection channels, and more (preventive) maintenance, as well as the possible effect on plant availability, are weighed against the higher level of protection obtained. Such trade-off studies are of particular importance for protection against localized core malfunctions, which could result in a large number of sensing channels if each fuel subassembly were to be individually monitored for temperature, flow, etc.

In order to improve the overall reliability of protection systems, it appears necessary2, as mentioned in the foregoing, to consider the development and installation of backup (or secondary) safety shutdown systems, which are completely adequate by themselves and which preferably are to be based on a principle for reducing reactivity different from that of the primary system. The work to be performed in support of the development of backup safety shutdown systems is to cover the following areas: (a) time response requirements in view of the various postulated accidents, (b) development of strategies to be used for actuating the primary and the backup safety shutdown systems, (c) establishment of reliability requirements for adequate protection (minimization of the possibility for common-mode failures), and to keep the rate of spurious reactor trips within economically tolerable limits, (d) structural dynamics studies in order to determine whether the backup safety shutdown system becomes incapacitated by distortion of the core, and if so, at what level of core deformation, (e) component and system development, and (f) system testing under realistic conditions. As regards item (b), strategies need to be worked out regarding the logic (e.g., signal-types and set-points) to be used for actuating the primary and the backup shutdown systems.25

Development of fully passive reactor safety shutdown systems, to be triggered only in case of failure of other safety shutdown systems, would enable to add an additional level of safety (in particular as regards human-induced errors). Such systems are quite feasible, and their development can be carried out to the level of proven performance within a limited number of years, without requiring excessively high funding levels.24

For the detection of localized core malfunctions, emphasis is to be placed on those sensing systems which, if tied into the protection system, give an
adequate level of protection, while still not requiring an overly high investment cost. Important in this respect are those detection methods for localized core malfunctions, which do not require a complete and separate reactor-trip channel for each fuel subassembly.16-18 Sensing systems which are good candidates to fulfill this latter requirement are: (1) fission product detection and location (FED&AL) systems, (2) acoustic noise detectors (or alternatively pressure pulse detectors, sensing the sharp and characteristic pressure pulses generated by vapor bubble collapse in the upper parts of the core or in the upper plenum), (3) anomalous reactivity detection systems, and (4) neutron flux noise analyzers. As mentioned earlier, FEDAL systems are expected to have a strong impact on the economic viability of LMFBRs. Strong emphasis should be placed on their timely development, not only for purposes of normal plant operation, but also for plant protection, in particular as regards single subassembly incidents. Probe-type FEDAL sensors with electrical output signals, capable of being installed on individual subassemblies and having a relatively fast response time (< 1 minute), would be extremely attractive; the heated-thermocouple probe for fission gas detection, proposed by Gast,12 falls in this latter category.

Acoustic sensing systems appear to be among the most promising for detection of coolant boiling. A distinction needs to be made, in this respect, between steady-state boiling in a limited region inside a fuel subassembly, and transient bulk boiling, accompanying coolant ejection, followed by vapor bubble collapse in the upper part of the subassembly or in the upper plenum. Steady-state boiling results in acoustic emissions which are not easily distinguishable from the background noise; however, the collapse of isolated sodium vapor bubbles causes generation of high pressure pulses, having a characteristic frequency spectrum, which allows relatively easy retrieval of the signal from the background noise.17,18,26,27

Emphasis is to be placed on experimental studies aimed at determining the performance of the various proposed sensing systems in connection with the fluid-mechanics and heat-transfer phenomena (including coolant ejection, vapor-bubble collapse, and coolant reentry), accompanying the early phases of the sequence of events which could lead to whole-core or local-core accidents. Such studies are, where possible, to be carried out in conjunction with on-going out-of-reactor and in-reactor experimental programs, using both metallic and nonmetallic (water, Freon, etc.) coolants.

Analytical and experimental studies are to be made regarding trade-offs between the sensitivity and the number of individual primary sensors, necessary for detection of particular single subassembly malfunctions and for meeting protection-system redundancy requirements. Such trade-offs, which can be effected by judicious mixing of subassembly coolant outlet flows (e.g., by means of specially designed instrument plates at the core outlet), could be considered in conjunction with thermocouples, probe-type flowmeters, probe-type fission-gas detectors, etc.23

A considerable effort is to be directed at the analysis of the reliability of the various possible protection systems, which are to be defined on the basis of the results of the above studies. These studies are to be aimed at determining the overall system reliability on both a comparative and an absolute basis, as well as at establishing permissible sensor failure rates in connection with the economically tolerable rate of spurious reactor trips or power-runbacks, for various assumptions regarding replacement and preventive maintenance (on-line and/or off-line). Criteria are to be developed for LMFBR protection systems, which are general enough to be applicable to all reactor systems, and which are to cover such aspects as redundancy, diversity, common-mode failures, single-component criterion, power-supply failure, etc.
6. **Conclusions**

Long-term reliance on nuclear fission reactors as a major energy supply source requires a continued effort, aimed at striving to approach the ideal situation of inherent safety, obtained predominantly by means of passive systems, also for the hypothetical case of core disassembly events. For the short term, and with the aim of (1) obtaining necessary LMFBR operating experience, (2) determining the economic viability of the overall concept, (3) allowing a normal evolutionary development process to take place, it will be necessary to rely on reactor safety shutdown systems (passive as well as active) in the study of hypothetical end-of-spectrum accidents. It is therefore, necessary to increase considerably the effort aimed at the development of reactor safety shutdown systems of high reliability and proven performance, while maintaining a considerable effort aimed at the study of end-of-spectrum accidents and their containment, preferably by means of passive systems.

The following non-exhaustive list gives areas where a continued effort is required in connection with LMFBR protective system development

(1) Continued development of sensing systems, in particular:
   (a) FEDAL systems,
   (b) pressure pulse, and/or acoustic, sensors,
   (c) anomalous-reactivity detectors, and
   (d) neutron flux noise analyzers.

(2) Test the performance of sensing systems under realistic accident conditions, by means of in-reactor and out-of-reactor experiments.

(3) Develop backup (or secondary) safety shutdown systems. Particular emphasis should be given to systems which are fully passive.

(4) Develop an overall strategy for protection against:
   (a) whole-core accidents initiated by high-frequency events (anticipated transients),
   (b) whole-core accidents initiated by low-frequency events,
   (c) local-core accidents (including single subassembly accidents), and
   (d) loss of capital investment.

(5) Perform reliability analyses concerning the various possible protection systems, both on a comparative and an absolute basis, including evaluation of the possibility for common-mode failures.

(6) Develop design criteria for LMFBR protection systems, in view of the requirements listed under (4). Revise and adapt current standards for plant protection systems, if and where necessary.

(7) Evaluate the impact on the long-term overall LMFBR economic viability, if a licensing position is maintained in which no credit is given to reactor power safety shutdown.

7. **Acknowledgements**

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8. References


Table 1. Categories of Safety Systems

<table>
<thead>
<tr>
<th>Safety System Description</th>
<th>Type of System</th>
<th>Type of Failures</th>
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<tbody>
<tr>
<td>Reactor Safety Shutdown Systems based on Passive Devices (e.g., Thermal, and/or Fissile Fuses)</td>
<td>Passive</td>
<td>Performance Failures</td>
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<tr>
<td>Core Blast Shields</td>
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<td>Containment Systems without Isolation Valves</td>
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<td>Core Catchers</td>
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<td>PAHR Systems not Requiring Pumps</td>
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<td>Reactor Safety Shutdown Systems or Plant Protection Systems (PPS)</td>
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<td>Reliability Failures</td>
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<td>Reactor Safety Boron Injection Systems</td>
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<td>ECCS Requiring Pumps</td>
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<td>Core Spray Systems Requiring Pumps</td>
<td>Active</td>
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<td>Containment Iodine Filter Systems</td>
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<td>PAHR Systems Requiring Pumps</td>
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<tr>
<th>Subassembly</th>
<th>Fault Protection (from R. D. Smith\textsuperscript{22})</th>
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<tr>
<td><strong>France</strong></td>
<td>Phéniix Future</td>
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<td>Thermocouple</td>
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Table 2. Subassembly Fault Protection (from R. D. Smith\textsuperscript{22})
RANDOM PIN FAILURES

DUE TO
1. DEFECTIVE CLADDING,
2. LOCAL BLOCKAGES,
3. ENRICHMENT ERRORS (SINGLE PIN)
(HIGH PROBABILITY DURING CORE LIFE TIME)

PIN-TO-PIN FAILURE PROPAGATION
DUE TO
1. FISSION-GAS RELEASE,
2. COOLING DEFICIENCY (DUE TO CRUD, FUEL WASHER, FUEL-COOLANT CHEMICAL INTERACTION),
3. RELEASE OF SMALL AMOUNTS OF MOLTEN FUEL,
LEADING TO EXTENSIVE INFERIOR LOCAL FLOW BLOCKAGES.

EXPERIMENTAL AND ANALYTICAL STUDIES, AS WELL AS CURRENT IRRADIATION DATA, SHOW RAPID AND EXTENSIVE PIN-TO-PIN FAILURE PROPAGATION TO BE OF EXTREMELY LOW PROBABILITY. STATISTICAL EVIDENCE TO BE FORTHCOMING FROM OPERATION OF DEMONSTRATION PLANTS

MINIMUM TIME REQUIRED FOR COMPLETE SUBASSEMBLY MELTDOWN:
~10 SEC. IN CASE OF A COMPLETE INLET BLOCKAGE

REDUCTION OF OVERALL SUBASSEMBLY FLOW

SUBASSEMBLY BULK COOLANT BOILING AND SUBASSEMBLY VOIDING

SUBASSEMBLY MELTDOWN

SUBASSEMBLY-TO-SUBASSEMBLY PROPAGATION

WIDE-CORE ACCIDENT

PLANT PROTECTION SYSTEM, HAVING PROBABILITY OF FAILURE < 10^-5 PER REACTOR YEAR, USING SIGNALS FROM
1. SUBASSEMBLY OUTLET THERMOCOUPLES,
2. ANOMALOUS REACTIVITY DETECTORS,
3. PRESSURE PULSE SENSORS, AND POSSIBLY ALSO
4. FEDAL SYSTEM,
WILL REDUCE PROBABILITY OF WHOLE-SUBASSEMBLY ACCIDENT (SUBASSEMBLY MELTDOWN) TO < 10^-6 PER REACTOR YEAR
Fig. 1 Schematic Representation of a Hypothetical Sequence of Events Leading, Via a Single-Subassembly Accident, to a Whole-Core Accident.
<table>
<thead>
<tr>
<th>Protection System 1</th>
<th>Protection System 2</th>
</tr>
</thead>
<tbody>
<tr>
<td><strong>Primary Purpose(s)</strong></td>
<td><strong>Protection against initiating events with potentially severe public-safety impact, including</strong></td>
</tr>
<tr>
<td></td>
<td>(1) anticipated transients as well as (2) lower-probability events.</td>
</tr>
<tr>
<td><strong>Principal Characteristics</strong></td>
<td>(1) strictly limited number of reactor scram channels ($&lt; 10$)</td>
</tr>
<tr>
<td></td>
<td>(2) unreliability, $\sim 10^{-7}$ per demand</td>
</tr>
<tr>
<td></td>
<td>(3) strictly controlled access for all vital components (installation possibly to be in vaults).</td>
</tr>
<tr>
<td><strong>Sensing Function</strong></td>
<td>Limited number of highly diverse sensing channels</td>
</tr>
<tr>
<td></td>
<td>Sensor Group 1</td>
</tr>
<tr>
<td><strong>Logic Function</strong></td>
<td>Hard-wired circuitry</td>
</tr>
<tr>
<td></td>
<td>Logic Section 1</td>
</tr>
<tr>
<td><strong>Actuating Function</strong></td>
<td>Actuator Group 1</td>
</tr>
<tr>
<td></td>
<td>Rapid shutdown of reactor</td>
</tr>
</tbody>
</table>

*Fig. 2 Schematic Representation of a Possible Strategy for Overall Protection System Design.*
The status of LMFBR safety instrumentation is reviewed with emphasis on in-vessel sensors which may be employed to avoid conditions leading to possible fuel failure propagation. Focus is placed on core-exit temperature and flow sensors, acoustic monitors, and FEDAL equipment planned for FFTF and the CRBRP. Special emphasis is given to possible instrumentation requirements based on data from failed fuel swelling measurements. Additional development work relating to instrumentation required for operation with failed fuel is suggested.

Introduction

One of the many factors in favor of the LMFBR as the optimum choice among competing concepts for central station power generation is its proven inherent safety and stability against serious consequences from potential accident-initiating mechanisms. Contributing to this inherent safety is the Doppler feedback phenomenon, demonstrated in the SEFOR program, the compatibility of oxide fuel, stainless steel cladding and sodium coolant over a wide range of conditions, as tested in EBR-II and elsewhere, and the effective heat removal capability of liquid sodium. While many areas of safety relating to core performance have been demonstrated by analysis and experiment, not all considerations have been fully explored. Thus, vigorous safety development programs including those in the FFTF will continue in support of the design and operation of the Clinch River Breeder Reactor Plant (CRBRP).

An important area of development activity in which safety concerns remain to be fully evaluated relates to the possible consequences of a power/flow mismatch within a subassembly. Safety requirements for in-vessel instrumentation are very strongly influenced by developments in this area of research. In this paper we wish to give emphasis to those instrumentation systems which may be considered in the context of the possibility and consequences of subassembly flow blockage, especially the type of propagating local subchannel blockage which may result from an initial fuel failure.

Safety Instrumentation System Development

The term "safety instrumentation," refers to the set of measuring devices which are employed to monitor plant conditions and to increase overall safety and plant availability. As noted above, the scope of this paper is concerned mainly with questions relating to core integrity and principally with in-vessel sensors, and their signal leadout and processing equipment, alarm and trip mechanisms and testing provisions. These devices individually may be utilized in a multiplicity of functions:

- Neutron detectors are used to assure reactor subcriticality during refueling, to monitor power levels through low,
intermediate and high ranges of operation, and to detect anomalous changes in power or reactivity.

- Temperature sensors are used to compute the plant heat balance and to measure or infer temperatures and temperature gradients in limiting structural components, such as vessel outlet nozzles and core elements.

- Flowmeters and level probes are employed to assure adequate cooling flow in all parts of the primary system. Flowmeters are also used to compute the plant heat balance.

- Failed fuel detectors monitor for released fission products and serve to characterize potential core damage levels and contamination effects.

- Acoustic noise monitors are deployed for the purpose of detecting anomalous vibrations, hardware impacts and bearing failure, and may serve as a method for detecting coolant boiling.

These instrumentation systems are incorporated as plant design features only after a considerable amount of development effort to determine: 1) technical basis, 2) functional requirements, 3) reliable design and performance. The technical basis for each safety instrument application is the possibility that a certain plant malfunction may produce hazardous conditions or conditions not within normal operating ranges. For example, a potential technical basis for core-exit temperature and flow monitoring instrumentation is the possibility of subassembly flow blockage either at the inlet, or at some position above within the subassembly, due to foreign material or because of damaged core hardware. As in the FERMI case, such flow loss could produce an unacceptable condition of local fuel melting and possibly even more severe consequences.

The lower internals of the FFTF and the CRBRP have been designed to tolerate a blockage of some inlet flow paths without significantly affecting the flow rate to the reactor assemblies. Planar blockages of less than 50% are believed to produce a subassembly gross temperature increase of less than 15°F, and planar areal blockages of greater than 50% are considered to be incredible. For these reasons, no technical basis is considered to exist requiring in-vessel instrumentation to protect against subassembly blockage conditions. As presently planned, core-exit thermocouples in the CRBRP are justified only for diagnostic purposes, i.e., to monitor and help evaluate plant behavior. Operation of the plant will not be dependent on the performance of this instrumentation.

In selecting the functional requirements of an instrumentation system, it is necessary to obtain a quantitative understanding of the accident sequence and to evaluate the required instrument sensitivity and response characteristics for initiating preventive action. Early estimates of required core exit instrumentation performance for the case of a large, rapid blockage indicated that thermal sensors with time constants of about 1 second should be developed. Figure 1 gives the results of an analysis of required thermocouple time constant, \( t \), and instantaneously responding flowmeter trip settings to prevent subassembly bulk outlet temperatures from reaching 1500°F for a matrix of rapid, gross flow-loss cases. If no subassembly-to-subassembly cross-flow is a factor, reliable flowmeters are seen to furnish superior capability. Effective mixing of flow is required to obtain the thermocouple performance shown.
CORE-EXIT INSTRUMENTATION RESPONSE TO FLOW BLOCKAGE ACCIDENTS

As a result of such analyses, thermocouple performance experiments were conducted in order to provide a reliable, effective system for the FFTF. The influence of thermowell and guide-tube enclosures was evaluated since maintenance and replaceability of these sensors is an important consideration. Typical data for the configurations tested is listed in Table I.

TABLE I

<table>
<thead>
<tr>
<th>THERMOCOUPLE/THERMOWELL/GUIDE TUBE COMBINATIONS INDICATED (IN SECONDS)</th>
<th>WET T/C</th>
<th>0.040-in. THERMOWELL</th>
<th>0.060-in. GUIDE TUBE</th>
</tr>
</thead>
<tbody>
<tr>
<td>1/8 in. GROUNDED JUNCTION</td>
<td>1000°F - 800°F</td>
<td>0.73</td>
<td>5.73</td>
</tr>
<tr>
<td>1/8 in. INSULATED JUNCTION</td>
<td>600°F - 850°F</td>
<td>1.57</td>
<td>7.82</td>
</tr>
<tr>
<td></td>
<td>1000°F - 800°F</td>
<td>1.55</td>
<td>6.75</td>
</tr>
<tr>
<td>1/16 in. INSULATED JUNCTION</td>
<td>1000°F - 800°F</td>
<td>0.266</td>
<td>2.87</td>
</tr>
</tbody>
</table>

Since a less demanding surveillance-only function is specified as the present requirement for both the FFTF and CRBRP, core-exit thermocouples are able to be placed in dry-well guide tubes; a time constant of 90 seconds is estimated for the FFTF design, depicted in Figure 2. This example also serves to illustrate how design and hardware development is determined by the functional requirements. Table II lists the variation in design choices in current LMFBR plants. Recently, consideration is being given to placing thermocouples only in selected representative positions (7-19) in a 1/6 sector of the CRBRP core, with a small number (∼3) positioned above selected locations in the other 5 sectors.
Figure 2

FFT CORE-EXIT INSTRUMENTATION

T/C AND FM LEAD WIRES 1 in. GUIDE TUBE

OUTLET FLOW PORTS (3)

SS SHEATHED THERMOCOUPLES 1/8 in. a.d. Cr-Al, MgO INSULATED

1 in. EDDY CURRENT FLOW METER (5-1/2 in. LONG)

4-1/2 in. a.d. INSTRUMENT TREE FLOW DUCT

CLEARANCE ~ 2 in. COLD, 1/2 in. HOT

FUEL ASSEMBLY DUCT

TO CORE CENTER 6 ft

Table II

CORE EXIT THERMOCOUPLE SYSTEMS IN LMFBR SYSTEMS UNDER CONSTRUCTION OR PLANNED

<table>
<thead>
<tr>
<th>Facility</th>
<th>Description</th>
</tr>
</thead>
<tbody>
<tr>
<td>FFTF</td>
<td>3 T/Cs PER FUEL ASSEMBLY IN WELL</td>
</tr>
<tr>
<td>CLINCH RIVER</td>
<td>1 T/C PER FUEL ASSEMBLY AND BLANKET ASSEMBLY IN WELL</td>
</tr>
<tr>
<td>PFR</td>
<td>1 T/C PER FUEL ASSEMBLY IN WELL</td>
</tr>
<tr>
<td>SNR</td>
<td>4 T/Cs PER FUEL ASSEMBLY IN WELL</td>
</tr>
<tr>
<td>PHOENIX</td>
<td>2 T/Cs PER FUEL ASSEMBLY IN WELL</td>
</tr>
</tbody>
</table>
Instrumentation Provisions Based on Subchannel Blockage Hypotheses

The currently reduced FFTF and CRBRP in-vessel safety instrumentation provisions are the result of a lengthy evaluation of the type discussed above. Initial concerns over gross power/flow mismatch occurrences have either been precluded by design (inlet blockage), administrative control (misloaded fuel) or by ex-vessel measurements (loss of pump power). Internal subchannel blockages due to lodged particulate is considered credible, but the configurations computed to cause fuel damage are not considered likely (e.g., >0.5 inch-long blockage of two adjacent subchannels with heat-generating material).

The problem of internal subchannel blockage is one which we suggest should receive further research since the limited data available on the performance of failed fuel gives concern that a mechanism exists for damaging blockage conditions to occur. If this type of damage can produce propagating fuel failures in highly irradiated fuel, additional safety instrumentation may be required. The basis for concern is that the separate constituents of such a propagating sequence are analytically credible or can be produced experimentally:

- The initiating mechanism, fuel failure, is an expected event
- Sodium logging has been observed in leaking fuel
- Low density sodium/fuel reaction products will produce swelling
- Rod swelling increases clad temperature in adjacent rods
- Increased clad temperature can shorten fuel life
- Swollen adjacent failures could produce local boiling in trapped sodium

Therefore, it may be learned that a functional requirement of such instrumentation should be to preclude the possibility of local molten-fuel-coolant-interaction (MFCI) type events.

Evaluations of expected fuel failure frequency have been made based on calculations of life-limiting mechanisms and from available thermal and fast reactor experience. Even minimum estimates of the number of failures per core fuel batch (~0.15%) may make it impractical to avoid operation with failures in both the FFTF and the CRBRP cores due to unacceptable losses in plant availability with present methods of locating and removing failures.

Probability and Consequences of Failed Fuel Swelling

The Breeder Reactor Operation (BRO) of the General Electric Company (GE) has conducted a number of irradiation tests to investigate the performance of failed fuel. Results from these tests (Figure 3) indicate that further definition of performance is warranted, since, despite the few tests performed, rod maximum diametral swelling by as much as 34% (ΔD/D) has been measured (volume increases are lower because of axial non-uniformity).
The few available swelling rate measurements show that, while the reaction completion may occur within one day, the potential for rapid propagation to adjacent fuel rods by this mechanism alone is low.

Further testing, however, may show that adjacent failures are promoted by the fuel swelling phenomenon by virtue of increased temperatures in neighboring rods. Creep rupture limits will be approached earlier in life in these rods, spacer contact loads may be increased, and fuel/clad mechanical interaction may be promoted resulting in premature failures.

Conclusions

Additional testing of defective fuel behavior under controlled conditions with carefully designed instrumentation and surveillance programs will provide invaluable information to select reactor operating limits for operation with defective fuel in the FFTF and CRBRP. The alternative approach of developing this information in the FFTF and CRBRP requires "bootstrapping" from a very conservative initial operating philosophy (such as detecting and removing even leaking fuel rods) to a more realistic operating philosophy that will result in plant availabilities consistent with central station power plant objectives. It is not clear at this time that such an approach is feasible, since the instrumentation needed to characterize failed fuel performance and stability during a "bootstrapping" mode of operation will not be available. Therefore, while it is not expected that operation with fuel failures will reduce plant safety, additional instrumentation capability, e.g., methods utilizing fission product release signals or acoustic noise from local boiling events, should be developed as a second level of confidence that safe operation is assured.
A Comparative Study of the Safety of Liquid Metal Cooled and Gas Cooled Fast Reactors

by

L. Cave

Pollution Prevention (Consultants) Ltd.

1. INTRODUCTION

Although sufficient work has been done in the United States and in Europe to demonstrate that the GCFR is technically feasible there is a marked difference in the amount of effort devoted to the development of the LMFBR on the one hand and the GCFR on the other. This difference is due partly to the belief in the early days of fast reactor development that only liquid metals could provide sufficient heat transfer to give an economic system, so that the LMFBR has had a longer development phase, and partly to the belief that, even though the heat transfer problem could be solved, the GCFR system could not give adequate safety. However, in spite of intensive efforts in many countries the safety aspects of the LMFBR system are still the subject of some criticism. Thus the present time appears to be an appropriate one at which to make a comparative study of the safety of the two systems.

To be meaningful a comparative study of safety should, as far as possible, be made on a quantitative basis within a framework which takes into account the fact that to improve the safety of a reactor is likely to increase its cost and could render it uncompetitive with rival systems. Although there are a number of uncertainties about both breeder systems that prevent the making of a completely quantitative study, the growing acceptance of the cost/benefit concept does lead to the possibility of making a useful comparison at the present time, using a relatively simple method, but it is not yet possible to take economic factors fully into account.

Ideally the study should compare safety in normal operation, as well as in accident conditions, but this is not possible within the confines of the present paper.

The principal features of the method used in this paper are as follows:

1. A quantitative standard of safety is defined which is common to both systems.
2. The possibility of attaining this standard of safety in each system with feasible design features, largely irrespective of cost, is examined.
3. The effect of substantial changes in the basic assumptions, including the standard of safety required, on design feasibility are investigated.
4. The effect on safety of the generating costs and doubling times predicted for the two systems are examined, taking into account factors which may lead to differences in availability.

The effects on safety of the differences between the main variants of each system (i.e., "pool" and "loop" type LMFBR; upward and downward core flow GCFR) are examined briefly but these are found to be unimportant.

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The quantitative analysis of reactor safety has been the subject of numerous papers since the publication of a report by Mulvihill and Reed (Ref. 1) in 1965. The concept of balancing risk and consequence in a quantitative manner, so that quantitative targets for the reliability required from reactor sub-systems could be established, was first discussed seriously in an open international forum at the I.A.E.A. symposium on reactor safety, held in Vienna 1967 (e.g., see Ref. 2 and Ref. 3), although the concept had already been adopted in Canada as early as 1965. The attitude of the regulatory bodies in most countries was one of scepticism but it is interesting to note that in the U.S., at least, the value of the concept has now been officially recognized so far as LWR safety is concerned by the publication of Document WASH 1270 (Ref. 4), the implications of which are discussed in Section 4, below. In the absence of an authoritative statement in quantitative terms as to the standard of safety required, the value of the quantitative approach is diminished as the designer has to fix his own targets and hope that these do not depart too far from the qualitative standard which his regulatory body has in mind. Even with this limitation the approach is a useful design tool, and provides a greater insight into the relative importance of various factors in the design and leads to a better balance between the various features of importance to safety. It has been extensively used in this way in the U.K. by design teams concerned with the AGR, HTR, SGHWR and LMFBR. Its application to the LMFBR in Italy and in FRG has also been reported.

As has been emphasized in previous papers by the present author (e.g., Refs. 5 and 6) at an early stage in the conceptual design it is pointless to adopt an over-elaborate method of analysis; the objective should be to use as simple a method as possible in order to define the reliabilities required from the various sub-systems and to show whether, in the light of previous experience with analogous equipment, it is reasonable to suppose that the necessary reliability could be attained. Analysis on these lines by the present author for thermal GCR, LMFBR and some aspects of GCFR have been reported in Refs. 5, 6, 7 and 8. This work has shown that useful results can be obtained by considering mainly the more severe accidents, i.e., those which could release a large proportion (10 to 30 percent of the fission product inventory) to atmosphere. Other published work relating to thermal GCRs and to LMFBR is contained, for example, in Refs. 9, 10, 11, 12 and 13. In some of these studies more complex methods have been used but the treatment is limited to particular protection systems.

In the 7-year period covered by this work it is inevitable that increasing experience of nuclear plant operation, together with improvements in analytical technique, should lead to some changes of opinion as to the reliability attainable by particular systems and where possible the results of the more recent studies have been used. Consequently, it is not considered necessary to present in this paper complete quantitative analyses for each of the sub-systems which make up the reactor systems which are being compared.

3. FUNDAMENTAL STEPS IN THE QUANTITATIVE ANALYSIS

In order to adapt existing work for the purposes of this paper it is convenient to define firstly the fundamental steps in the method of quantitative analysis which meets this requirement. These are as follows:

(a) Definition of the safety requirement in quantitative terms.

(b) Evaluation of the frequency of primary faults for the conceptual design.

(c) Determination of the reliability required from the main sub-systems in order to discover whether a design can be defined which is compatible with (a) and (b) and which is technically feasible in terms of current practice or possible development.

(d) Consideration of the interrelation between safety and economics.

As indicated in Section 2, above, it is sufficient at the conceptual design stage...
of thermal reactors to concentrate on the risk of large releases to atmosphere, thus permitting a simple definition of the safety target. However, in the case of fast reactors it is necessary to consider whether the greater potential risk of partial core vaporization requires the definition of different limits of risk for accidents which could lead only to core meltdown, as distinct from those which also could lead to core vaporization. These limits might differ by a factor of at least 10. A method for incorporating this and other complicating factors is described in the next Section.

In evaluating the frequency of primary faults it is necessary to consider the reactor in relation to the rest of the power station and the external electrical system to which it is connected. If this is not done fallacious assumptions can readily be made as to the frequency of important faults such as loss of station generation combined with loss of off-site electrical supplies.

As the frequency of primary faults is dependent in part on the balance of plant design, as well as that of the reactor itself, an iterative process may be required, as changes in design, to secure the necessary reliability in one sub-system, may have an adverse effect on another sub-system.

The main sub-systems whose reliability must be considered in relation to the safety of the system are as follows:-

(i) The reactor shut-down system.

(ii) The main and emergency cooling systems, including the necessary power and fluid supplies.

(iii) Structures whose failure could effect (i) or (ii) above.

(iv) The containment system, including any core-catchng device which is required to protect the containment itself in the event of a core meltdown.

The application of these various steps in the analyses for LMFBRs and GCFRs are described in the following sections.

4. DEFINITION OF SAFETY REQUIREMENTS IN QUANTITATIVE TERMS

In previous work the author of the present paper has assumed that an acceptable "target frequency" for the release to atmosphere of a large proportion (e.g., 10 percent or more) of the volatile and gaseous fission products stored in a 1000 MWe reactor on a rural site is about $10^{-8}$ per reactor year. However, in comparison with the views expressed by UKAEA at the Julich symposium on safety in February 1973 (Ref. 26), in relation to thermal reactors generally, and with those expressed more recently by the USAEC in "WASH 1270" (Ref. 4) in relation to LWRs, this target appears to be unnecessarily conservative. Due to the additional hazards associated with faults leading to core vaporization in fast reactors which must be considered as a potential risk, the targets recently proposed for thermal reactors are not directly applicable. However, as described in Appendix 1 to this paper, a target suitable for fast reactors can be derived from the thermal reactor case. The target is expressed in a manner which facilitates inclusion in the analyses of factors such as the lack of independence of the secondary containment in severe fault conditions and the higher absorption of some fission products in an LMFBR, due to the sodium, than in a GCFR.

The target is expressed in the form

$$\sum_{i} n_{i} \leq E$$

where $s_{i}$ represents the probability of a particular fault sequence, which includes the primary fault.

$n_{i}$ represents a weighting function appropriate to the nature and conditions of the release of activity due to that sequence.

$E$ represents the maximum "expectation of damage" which is acceptable for the reactor.

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E could be expressed in terms of the additional number of potential cancer cases due to the reactor but by extrapolation from the thermal reactor target and a change in the form of the weighting function we can write, in the limit, \( \sum w_i = 1 \), where \( w_i \) is a dimensionless weighting factor. In principle the summation extends over all fault sequences but in practice, in a preliminary analysis, it is only necessary to consider those which terminate in core melt down. A further discussion of the method of deriving this target is contained in Appendix I.

5. EVALUATION OF FREQUENCY OF PRIMARY FAULTS FOR THE CONCEPTUAL DESIGNS

5.1 GCFR

The frequency of primary faults in various designs of large gas cooled reactors of different types has been discussed in a number of papers (e.g., Refs. 6, 8 and 9). Inevitably there are differences between the estimates, due partly to differences in design and partly to differences in the interpretation of the available data by the authors concerned. However, there is reasonable agreement on the following points:

(a) Frequency of loss of coolant accidents (leak area near the design limit; say 25 to 100 sq. in.), \( 10^{-3} \) per reactor year. Smaller leaks, of lesser significance, may have higher frequencies.

(b) Frequency of reactor trips, including those of an innocuous or of a spurious nature, \( \sim 3 \) reactor year.

(c) Frequency of main turbine trips, \( \sim 3 \) per reactor year.

(d) Loss of off-site (i.e., grid) electrical supplies due to faults unconnected with the nuclear power station occurs about 0.3 times per year, but due to the effect of loss of the nuclear power station (NPS) generation on the system the frequency of loss of the off-site supplies in the event of an NPS trip is about \( 10^{-2} \) per event.

(e) Frequency of sequential loss of all main electrical supplies (i.e., off-site plus unit generation), due to the combined effects of (b), (c) and (d) is between 0.03 and 0.1 per reactor year.

(f) Frequency of reactivity excursions at power requiring rapid shutdown of reactor, \( 10^{-3} \) per reactor year.

(g) Frequency of other severe faults requiring reactor trip (e.g., loss of main boiler feed pumps; failure of circulator speed control) about 0.1 per reactor year.

It has been assumed initially that the frequencies of failure of:

(i) the PCRV (which is a common feature of all the GCR designs discussed)

(ii) the primary and back-up closures, or holddown devices, of the larger penetrations of the PCRV

(iii) major internal structures, such as the core support, whose failure could lead to core meltdown, irrespective of the reactor protection systems, or would prevent the operation of those systems can be made sufficiently small to meet the target frequency required. This aspect is discussed further in Section 6.5, below.

Because of the severe thermal stresses which could develop in a gas-cooled reactor operating with core inlet and outlet temperature of about 300°C and 650°C respectively, it is usually desirable to reduce core flow rapidly in the event of a reactor trip. Thus, so far as the safety of the plant itself is concerned, the frequency of demand for core cooling by "abnormal" means is quite high (\( \sim 3 \) per reactor year), whereas the frequency of demand for "emergency" core cooling to protect the public can be much lower (e.g., less than 0.1 per reactor year).
for designs with electrically driven circulators and still lower with steam
 driven circulators. It is for this reason that the reliability and flexibility
of steam driven circulators (or of independent turbo-alternators, taking steam
directly from auxiliary loops in the primary circuit, to drive the main circula-
tors, (as in Ref. 8) is attractive, particularly if a core configuration with
downward flow is used.

It should be noted that in none of the gas cooled systems, other than the early
"Magnox" type, does a fuel failure necessitate a rapid trip of the reactor.

5.2 LMFBR

In the case of LMFBR there is a discussion of the frequency of primary faults in
Ref. 7 and some more recent data in Ref. 11. In general the frequencies should
be similar except for the following:-

(I) Reactor trips

Because of the uncertainty arising from the effect of sub-assembly faults it is
likely that moderately severe fuel failures will necessitate a reactor trip.
Although the frequency of such failures should not be high (of order 1 per
reactor year) the extent of instrumentation required is likely to increase the
spurious tripping rate. Overall the rate might be expected to rise from about 3
for the GCFR to about 10 per reactor year.

In order to limit thermal stresses following a reactor trip deliberate reduction
of core flow is almost certain to be required, thus adding to the total number
of demands as is the case of GCFR.

(II) Loss of main pumps

In an LMFBR the option of using steam driven pumps to obtain high reliability is
unattractive owing to the need for an intermediate secondary sodium loop, due to
the sodium/water reaction. Thus the demand for core cooling in an emergency
situation is likely to be tied to the overall frequency of loss of main electrical
supplies, i.e., between 0.03 and 0.1 per reactor year.

(III) Loss of cooling loops

The possibility of sodium/water reactions in the secondary loop introduces an
additional type of fault; in its milder forms this can be considered analogous
to boiler tube failure in a GCFR, in terms of frequency, but is more likely to
represent a trip condition.

5.3 Summary of frequencies of primary faults

The frequencies of primary faults to be expected in the two fast reactor systems
may therefore be summarised as shown in Table 5.1.

6. RELIABILITY REQUIRED FROM THE REACTOR PROTECTION SYSTEMS AND REACTOR
STRUCTURE

6.1 Method of analysis

In order to use the quantitative safety target derived in Appendix I it is con-
venient, in the initial stages of the analysis, to sub-divide the target expecta-
tion equally between the broad groups of fault sequences shown below. It will
be seen that each of these groups is based on the failure of a reactor protection
system, or the critical structures. The groups are as follows:-

(i) Faults in which the reactor fails to trip promptly in response to some
other fault which leads to core meltdown if the reactor is untripped.

(ii) Faults in which the reactor has tripped but there is a failure to maintain
sufficient core cooling to prevent meltdown.

(iii) Structural failures which can prevent either the reactor shutdown system
Table 5.1 Estimated frequency of main primary faults in: (i) A typical GCFR design embodying features such as a PRCV with double closures, or hold-downs, steam or electrically driven circulators & a power cut back system to avoid unnecessary trips; (ii) A typical LMFBR.

<table>
<thead>
<tr>
<th>Serial No.</th>
<th>Type of Fault</th>
<th>Frequency of event per r.y.</th>
<th>Remarks</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td>GCFR</td>
<td>LMFBR</td>
</tr>
<tr>
<td>1</td>
<td>Innocuous or spurious reactor trips</td>
<td>3</td>
<td>10</td>
</tr>
<tr>
<td>2</td>
<td>Main turbine trip</td>
<td>3</td>
<td>3</td>
</tr>
<tr>
<td>3</td>
<td>Other faults requiring power cut back</td>
<td>3</td>
<td>3</td>
</tr>
<tr>
<td>4</td>
<td>GCFR. Depressurization accident, leak area near design value (25 to 100 sq.in., say)</td>
<td>$10^{-3}$</td>
<td>N/A</td>
</tr>
<tr>
<td>4a</td>
<td>LMFBR. Loss of coolant loops: (i) Loop</td>
<td>N/A</td>
<td>$10^{-3}$</td>
</tr>
<tr>
<td>5</td>
<td>(ii) Pool</td>
<td>N/A</td>
<td>$10^{-3}$</td>
</tr>
<tr>
<td>6</td>
<td>GCFR depressurization accident more severe than design</td>
<td>$&lt;10^{-7}$</td>
<td>N/A</td>
</tr>
<tr>
<td>7</td>
<td>LMFBR total loss of coolant accident</td>
<td>N/A</td>
<td>$&lt;10^{-7}$</td>
</tr>
<tr>
<td>8</td>
<td>Sudden severe tube failure in steam generator</td>
<td>$10^{-3}$</td>
<td>$10^{-3}$</td>
</tr>
<tr>
<td>9</td>
<td>Multiple tube failure in steam generator</td>
<td>see remarks</td>
<td>see remarks</td>
</tr>
<tr>
<td>10</td>
<td>Reactivity faults:</td>
<td>$10^{-2}$-$10^{-3}$</td>
<td>$10^{-2}$-$10^{-3}$</td>
</tr>
<tr>
<td></td>
<td>(i) Rod withdrawal of power</td>
<td>$10^{-3}$-$10^{-4}$</td>
<td>$10^{-3}$-$10^{-4}$</td>
</tr>
<tr>
<td></td>
<td>(ii) Rod withdrawal during startup</td>
<td>$10^{-3}$-$10^{-5}$</td>
<td>$10^{-4}$-$10^{-5}$</td>
</tr>
<tr>
<td></td>
<td>(iii) Reactivity withdrawal due to operator error whilst shut down</td>
<td>$10^{-4}$-$10^{-5}$</td>
<td>$10^{-4}$-$10^{-5}$</td>
</tr>
<tr>
<td></td>
<td>(iv) Subasemblony faults requiring trip action</td>
<td>see remarks</td>
<td>1</td>
</tr>
<tr>
<td>11</td>
<td>Other severe faults requiring reactor trip</td>
<td>$10^{-1}$</td>
<td>$10^{-1}$</td>
</tr>
<tr>
<td>12</td>
<td>Loss of off-site power</td>
<td>$0.3$</td>
<td>$0.3$</td>
</tr>
<tr>
<td>13</td>
<td>Failure of secondary containment to provide leak-tightness, on demand, within factor 100 of design value</td>
<td>$10^{-3}$</td>
<td>$10^{-3}$</td>
</tr>
</tbody>
</table>

*Not necessarily a trip condition
or the core cooling systems from functioning sufficiently to prevent core meltdown.

In each case core meltdown may, in certain conditions, be accompanied by a nuclear or fuel/coolant interaction (FCI) explosion.

It is shown in Ref. 8 that the assumption of an equal division of the expectation provides a good starting point for the optimization of the design, which, inevitably must be an iterative process.

Within each group of fault sequences the individual sequences must then be identified and the probability of each assessed, together with the appropriate value of the weighting function described in Appendix II. As the weighting function is dependent partly on the extent to which the release to atmosphere is delayed it is convenient to include the behaviour of the core catcher, if provided, and the secondary containment in each fault sequence.

In a broad survey of the type presented here it is sufficient to assume that all 'failure to trip' faults, for example, lead to the same set of possible sequences subsequently. Thus the annual frequency for that class of fault can be applied to each of the associated sequences. With this simplification it is shown in Appendix II that we can write

\[ f_x = \frac{0.3}{n_x \sum p(j_1) \ldots p(j_2) \ldots w_j} \tag{Eqn. 6.1} \]

where \( f_x \) denotes the maximum acceptable frequency of failure on demand of protection system 'x',

\( n_x \) denotes the annual frequency of primary faults which require protection system 'x' to function

\( p(j_1), p(j_2), p(j_3) \ldots \) denote the probabilities of events 1, 2, 3 ... which form the sequence 'j', subsequent to the primary fault and failure of the protection system

\( w_j \) denotes the weighting factor which is appropriate to sequence 'j'.

In the case of the critical structures a slightly different expression is required, as described in Appendix II, but the same principle is used.

6.2 Reliability required from reactor trip system

6.2.1 Summary of analysis

As described in the previous section, the first stage in the calculation of the reliability required from the reactor trip system is to define the fault sequences which can lead to core meltdown followed by a release to atmosphere. The main sequences are summarized in Table 6.2 (see Serial Nos. 4 to 10, inclusive) but in those where the primary containment is intact initially it is necessary to consider the further stages in which firstly, failure of the primary containment, due to action of the molten fuel, occurs within the period 0 to 10 hours and secondly failure occurs within the period 10 to 100 hours. It is assumed that the additional delay time provided by the secondary containment is negligible by comparison. These sequences, of course, imply failure of any core catching device which is provided. In those sequences where the secondary containment remains intact initially it is necessary, in principle, to consider the subsidiary sequence in which, owing to some defect, the containment has a high leak rate at the time of the fault. However, in practice, these do not make a substantial contribution to the total risk. The weighting functions appropriate to the various sequences are also listed in Table 6.2 (see Note 8 to Table) together with other necessary data, such as the annual frequency of faults which require a rapid reactor trip.
Table 6.2 Summary of assumed values for frequencies and probabilities of various events used in calculation of reliability required from reactor shutdown system

<table>
<thead>
<tr>
<th>Serial No. (a)</th>
<th>Event (b)</th>
<th>Frequency or Probability</th>
<th>Remarks (f)</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>Fault conditions requiring rapid reactor trip</td>
<td>LMFBR 1.2 per ry GCFR (upflow) 0.2 per ry GCFR (downflow) 0.2 per ry</td>
<td>See Note 1</td>
</tr>
<tr>
<td>2</td>
<td>Failure rate of secondary containment (self failure)</td>
<td>LMFBR $10^{-3}$ per d'd GCFR (upflow) $10^{-3}$ per d'd GCFR (downflow) $10^{-3}$ per d'd</td>
<td>See Note 2</td>
</tr>
<tr>
<td>3</td>
<td>Failure rate of core catcher (a) 0-10 hr (b) 10-100 hr</td>
<td>LMFBR $10^{-1}$ per d'd GCFR (upflow) $10^{-1}$ per d'd GCFR (downflow) $10^{-1}$ per d'd</td>
<td>See Note 3</td>
</tr>
<tr>
<td>4</td>
<td>Nuclear explosion (NE) of sufficient violence to cause immediate failure of primary &amp; secondary containments, if reactor fails to trip</td>
<td>LMFBR $10^{-2}$ per event GCFR (upflow) $10^{-3}$ per event GCFR (downflow) $10^{-4}$ per event</td>
<td>See Note 4</td>
</tr>
<tr>
<td>5</td>
<td>FCI explosion of sufficient violence to cause immediate failure of primary and secondary containments, if reactor fails to trip</td>
<td>LMFBR $10^{-3}$ per event GCFR (upflow) zero GCFR (downflow) zero</td>
<td>See Notes 5 and 8</td>
</tr>
<tr>
<td>6</td>
<td>NE as in 4, and immediate failure of primary containment, but secondary containment does not fail immediately due to explosion</td>
<td>LMFBR $3 \times 10^{-2}$ per event GCFR (upflow) $10^{-2}$ per event GCFR (downflow) $10^{-3}$ per event</td>
<td>See Notes 4, 6 and 8</td>
</tr>
<tr>
<td>7</td>
<td>FCI exp. as in 5 &amp; immediate failure of p. containment but secondary containment does not fail immediately due to exp.</td>
<td>LMFBR $10^{-2}$ per event GCFR (upflow) zero GCFR (downflow) zero</td>
<td>See Note 8</td>
</tr>
<tr>
<td>8</td>
<td>NE, but containments do not fail immediately due to explosion</td>
<td>LMFBR $10^{-1}$ per event GCFR (upflow) $3 \times 10^{-2}$ per event GCFR (downflow) $3 \times 10^{-3}$ per event</td>
<td>See Notes 6,8</td>
</tr>
<tr>
<td>9</td>
<td>FCI Exp. but containments do not fail immediately due to explosion</td>
<td>LMFBR $10^{-1}$ per event GCFR (upflow) zero GCFR (downflow) zero</td>
<td>See Notes 6,8</td>
</tr>
<tr>
<td>10</td>
<td>Core meltdown without explosion, if reactor fails to trip</td>
<td>LMFBR $\sim 0.85$ per event GCFR (upflow) $0.96$ per event GCFR (downflow) $0.996$ per event</td>
<td>See notes 7,8</td>
</tr>
</tbody>
</table>
NOTES ON TABLE 6.2

1. In case of LMFBR frequency of requirement for rapid trips is larger owing to subassembly fault. In case of GCFR frequency is governed by "loss of main electrical supplies" (0.6 per ry) & "other severe faults" (0.1 per ry).

2. Failure rate of $10^{-3}$ per demand is as low as can reasonably be expected for a relatively complex system without redundancy in all features.

3. It is assumed that if core catcher fails, secondary containment fails in a time of order 1 hour. In present state of knowledge failure rates for core catchers are difficult to assess, but typical concrete thicknesses alone should provide a useful delay, hence failure frequencies of $10^{-1}$, within 10 hours and $8 \times 10^{-1}$ in period 10 to 100 hours (i.e., probability of successful operation for at least 100 hours is 0.1 per demand), are not unduly optimistic.

4. (i) In this and subsequent serials the numbers in Cols. (c), (d) and (e) are the estimated probabilities of the complete sequences, following a primary fault and failure to trip.
   (ii) Relatively high figure for LMFBR reflects (i) margins for error in the complex calculations to estimate yield and structural effects, (ii) lack of large inherent pressure-resisting capacity of primary circuit and secondary containment. In typical GCFRs, pressure circuit is designed for about 100 bars as compared with 10 bars for LMFBR. Thus effect of the same size of pressure pulse is markedly less. Similarly secondary containment in GCFR is designed for several bars, to utilize back pressure, whereas tendency in LMFBR is to design for relatively low pressure resulting from sodium/air reaction. Lower frequency for downward flow GCFR, as compared with upward flow variant, based on better self-clearing characteristics of core.

5. Lower frequency of failure, as compared with nuclear explosion case, is based on assumption that potential maximum energy is substantially lower and explosion is less violent.

6. It is necessary to consider separately the cases in which a nuclear explosion and a FCI explosion does not cause failure of primary containment in order to allow for effect of the higher weighting factor, if failure should occur later due to failure of core catcher, in the former case.

7. If reactor fails to trip meltdown is assumed to be inevitable, with or without an explosion.

8. The weighting factors assumed in the case of LMFBR and GCFR, for the various conditions, are as follows:

<table>
<thead>
<tr>
<th>Case</th>
<th>Condition</th>
<th>LMFB</th>
<th>GCFR</th>
<th>Case</th>
<th>Condition</th>
<th>LMFB</th>
<th>GCFR</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>Nuclear explosion, with immediate release</td>
<td>$10^{-7}$</td>
<td>$10^{-7}$</td>
<td>7</td>
<td>Core meltdown, with immediate release</td>
<td>$10^{-6}$</td>
<td>$10^{-6}$</td>
</tr>
<tr>
<td>2</td>
<td>&quot; &quot; &quot; &quot; , release delayed 0-10 hr</td>
<td>$10^{-6}$</td>
<td>$3 \times 10^{-6}$</td>
<td>8</td>
<td>Core meltdown, release delayed 0-10 hrs</td>
<td>$10^{-5}$</td>
<td>$3 \times 10^{-5}$</td>
</tr>
<tr>
<td>3</td>
<td>&quot; &quot; &quot; &quot; , release &quot; 10-100 hr</td>
<td>$10^{-5}$</td>
<td>$10^{-5}$</td>
<td>9</td>
<td>Core meltdown, release delayed 10-100 hrs</td>
<td>$10^{-4}$</td>
<td>$10^{-5}$</td>
</tr>
<tr>
<td>4</td>
<td>FCI explosion, with immediate release</td>
<td>$10^{-6}$</td>
<td>-</td>
<td>10</td>
<td>&quot; &quot; &quot; &quot; , release delayed 0-10 hr</td>
<td>$10^{-5}$</td>
<td>$10^{-5}$</td>
</tr>
</tbody>
</table>
Because of the difference in the estimated probability of nuclear explosion between the upward and downward flow variants of GCFR these have been treated separately but it has not been considered necessary to differentiate between the 'pool' and 'loop' variants of LMFBR.

Clearly judgment of a subjective nature has to be exercised, at this stage of development of both systems, in compiling the data shown in Table 6.2 but the intention has been to avoid excessive optimism and excessive pessimism so that as useful a framework as possible is created from which the effect of perturbations of the assumed values can be determined. Using the method described in Appendix II and the numerical data in Table 6.2, the maximum acceptable failure rates for the reactor shut-down systems in LMFBR and GCFR have been estimated. The results are as follows:—

<table>
<thead>
<tr>
<th>System</th>
<th>Failure Rate</th>
</tr>
</thead>
<tbody>
<tr>
<td>LMFBR</td>
<td>$0.2 \times 10^{-5}$ failure per demand</td>
</tr>
<tr>
<td>GCFR (upward flow)</td>
<td>$0.9 \times 10^{-5}$ failure per demand</td>
</tr>
<tr>
<td>GCFR (downward flow)</td>
<td>$1.0 \times 10^{-5}$ failure per demand</td>
</tr>
</tbody>
</table>

As indicated above, these results should be regarded primarily as illustrative: the following comments, based on an examination of the detailed calculations, may be helpful in interpreting the results more generally.

6.2.2 Factors affecting trip reliability required in LMFBR

(i) The trip reliability required is dominated by the nuclear explosion case in which there is immediate failure of both primary and secondary containment. If the probability of this sequence could be reduced from $10^{-2}$ to $10^{-4}$ it would cease to be significant and the main requirement would arise from the core melt-down case, following a nuclear explosion without immediate damage to the containments.

(ii) The case of core melt-down without explosion is always significant (contributing not less than 5 percent of the demand) as a low reliability has been attributed to the core catcher.

If no core catcher is provided, core meltdown without explosion would be almost as important as the nuclear explosion followed by immediate failure of the primary and secondary containments. This conclusion is subject to the proviso that the 'weighting factors' attributed to the two types of release are correct; as a factor 10 separates the two cases it seems unlikely that the treatment of the meltdown case is overly pessimistic by a factor of more than 10.

(iii) The reliability of the secondary containment, in terms of freedom from system faults leading to high leak rate, has no significant effect as its contribution to safety is overshadowed by that of the core catcher, i.e., there is no point in providing a secondary containment which is substantially more reliable than the core catcher, so far as faults involving a failure to trip are concerned.

6.2.3 Factors affecting trip reliability required in GCFR

(a) GCFR (upward core flow)

(i) The case of nuclear explosion with immediate failure of primary and secondary containment is of about the same importance as that of core meltdown without explosion. However, if no core catcher were provided and there were little hold-up by the PCRV the latter case would be dominant, leading to a four-fold increase in the reliability required from the trip system.

(ii) As in the case of the LMFBR, the reliability of the secondary containment should be matched to that of the core catcher to secure a balanced design, so far as faults involving a failure to trip are concerned.

(b) GCFR (downward core flow)

(i) In this system the smaller probability attributed to the sequence 'nuclear explosion with immediate failure of primary and secondary containment' makes
this case barely significant compared with that of core meltdown without nuclear explosion. If no core catcher were provided it would be even less important.

(ii) The same comment applies as at a(ii).

6.3 Reliability required for continuity of coolant flow

In this case we are concerned only with situations in which the reactor has already tripped, as operation of the emergency cooling systems is irrelevant if the reactor has not tripped. It is assumed that sufficient shut-down margin is provided, in a refractory form, to prevent a nuclear explosion if the fuel melts and becomes concentrated in the lower part of the core or at the bottom of the tank or PCRV.

With these assumptions we are concerned in the case of the LMFBR with the effects of sodium vapour explosions resulting from fuel-coolant interactions and of core meltdown without explosion. In the case of the GCFR we have only the latter situation to consider. In both cases the conservative assumption is made that following any fault sequence leading to a core explosion or core meltdown no further credit can be taken for the normal or emergency cooling systems other than for any definable function as part of the core catcher system.

Considering the data required for the type of calculation described in the previous section, the only change is in the frequencies of the primary faults. In order to ensure a conservative approach it is desirable to consider initially the frequency of events in which continuity of flow must be maintained after a major change in operating conditions, even though the demand may be met in part by continued operation of the main loops. Using this approach reactor trips, irrespective of their cause, and a proportion of the main turbine trips-- as these can lead to reactor trips if control action is ineffective-- must be considered; from the data in Table 6.1 they are found to be the dominant source of demands for continuity of cooling after a major change of operation conditions. For LMFBR it is assumed that the frequency of such events is 11 per reactor year and for GCFR (both versions) it is assumed that the value is 4 per reactor year.

Using the method described in Appendix II it is found that the maximum acceptable rates for failure to maintain continuity of cooling, after a major change in operating conditions, are as follows:

LMFBR \hspace{1cm} 2.0 \times 10^{-6} \text{ per demand}

GCFR (upward flow) \hspace{1cm} 0.7 \times 10^{-6} \text{ per demand}

GCFR (downward flow) \hspace{1cm} 0.7 \times 10^{-6} \text{ per demand}

It should be noted that the special case of the loss of coolant accident in the GCFR does not have any significant effect on these rates, if the other frequencies and probabilities in Tables 6.1 and 6.2 retain the values shown.

As in the previous section these results should be regarded primarily as illustrative; the following comments, based on examination of the detailed calculations may be helpful in interpreting the results more generally.

(a) LMFBR

(i) The reliability required is dominated by the core meltdown case, the FCI explosion case contributed less than 20 percent of the demand.

(ii) If no core catcher were provided at all the reliability required would be increased by a factor of about 3.

(iii) As in the failure-to-trip case, high reliability of the secondary containment is of little value unless it is matched by that of the core catcher.

(b) GCFR

The assumptions made in the analysis are such that the performance of the core catcher is the dominant factor, once core meltdown has occurred.
6.4 Reliability required from the reactor containment and core catcher

The reactor containment may be regarded as consisting of the envelope of the primary coolant circuit, the secondary containment and any core catcher device which is provided to protect the primary containment (i.e., the primary circuit) or the secondary containment from damage by material from a melted-down core.

It has been assumed in the illustrative example used earlier in this Section that if the reactor is in normal operation the primary containment is effective. Strictly speaking account should be taken of the possibility that the primary circuit is in a weakened condition at the time of some severe fault (i.e., it would fail more readily than expected) but this refinement has been omitted in the example. In a rigorous analysis it would also be necessary to show quantitatively that the frequencies of failure of the major structures were consistent with the assumptions. These aspects are discussed briefly in Section 6.5 below.

In the illustrative examples of Sections 6.2 and 6.3 it has been assumed that the failure rate of the core catcher, within the first 10 hours is $10^{-1}$ per demand and $8 \times 10^{-1}$ per demand in the period 10 to 100 hours. This should not be a difficult requirement to meet in practice; for example it could well take a period of about 10 hrs for the molten material to make its way through the base slab of a typical PCRV. It has also been assumed that the core catcher would provide sufficient dispersion of the molten core material and sufficient heat removal to prevent boiling and the associated rapid evaporation of the more refractory fission products and the plutonium isotopes.

It has been postulated in the illustrative example of Sections 6.2 and 6.3 that the reliability of the secondary containment system, in the sense of retaining a high proportion of its design leak tightness, cooling capacity, etc. whilst the reactor is operating normally, would be represented by a failure rate of $10^{-2}$ per demand. However, from the discussion of the results of the illustrative analysis and some preliminary consideration of the effects of major changes in the input data it appears that a failure rate of $10^{-2}$ per demand would be adequate, in the severe fault conditions considered. Nevertheless it is possible that some minor fault conditions could arise in which the higher reliability would be advantageous in meeting the criteria applicable in such cases.

6.5 Reliability required from the reactor structures

Failure of certain of the principal structures of either an LMFBR or a GCFR would lead to situations in which the reactor protection systems would be inoperable or ineffective in preventing core meltdown, with the attendant risk of a nuclear or FCI explosion. Consequently the maximum failure rates of these structures must be compatible with the total risk acceptable from the reactor; as indicated in Appendix II, the necessary condition can be written $\Sigma f_i w_i = 0.3$, where $f_i$ is the frequency of structural failure of type $i$ and $w_i$ is the appropriate weighting factor for the ensuing release due to that failure.

It is not necessary to include in this summation those structural failures which do not interfere with the operation of the reactor shut-down system or the emergency cooling system, as these are included amongst the primary faults whose frequency is used to estimate the reliability required from those protective systems.

In the case of LMFBR core support failure, the data listed in Table 6.2 are assumed to apply but in the case of simultaneous failure of primary tank and leak jacket, which could conceivably result from seismic damage, complete loss of sodium would increase the risk of nuclear explosion and the primary containment would be breached at the outset. It is assumed that the probabilities of nuclear explosion, FCI explosion and core meltdown without explosion are 0.3, 0.3 and 0.4 per event, respectively. No delay in the release of active material in this core meltdown case is assumed owing to the extremely high risk of a sodium fire. Assuming a target figure of 0.15 for each case it is found that the
maximum acceptable frequency for core support failure is $10^{-6}$ per r.y. and for failure of the primary tank and leak jacket it is $4 \times 10^{-8}$ per r.y. It is assumed in this analysis that other major structural failures (e.g., failures of the primary tank alone or of internal and external ducts) would not interfere directly with the functioning of at least one of the reactor shut-down systems and would not prevent the removal of decay heat by at least one of the means provided. It is implicit in this assumption that the primary tank would be adequately isolated from the effects of sodium-water reactions, i.e., that some form of intermediate heat exchanger is employed and that the system provided to detect gross failures and dump the contents of a defective loop has adequate reliability.

In the case of GCFR complete failure of the vessel would lead to core dispersal, rather than to core compaction, thus fuel melting without explosion would be expected. However, failure of the core support structure, due to either a latent defect in the structure itself, or to a gross overload caused by aerodynamic forces following complete failure of a large closure, could lead to a nuclear explosion in addition to fuel melting. In order to proceed as in the previous cases it is necessary to make some different assumptions from those made in Sections 6.2 and 6.3, as the behaviour of the PCRV and the secondary containment would be altered. The principal changes for the case of failure of the core support due to latent defects are as follows:

(a) Nuclear explosion of sufficient violence to cause immediate failure of primary and secondary containment

(b) As (a) except that secondary containment does not fail immediately

(c) Nuclear explosion without immediate failure of the primary or secondary containment

No distinction is made between upward and downward core flow designs.

For the case in which failure of the core supports is assumed to be due to rapid depressurization it is assumed that the above frequencies are all greater by a factor of 10, as the primary containment would be breached by the primary fault.

As there are 3 quite distinct cases of structural failure to be considered, the sub-target of 0.3 is divided equally between them.

With these assumptions it is found that the maximum acceptable failure rates are as follows:

- Complete failure of PCRV: $1 \times 10^{-7}$ per r.y.
- Failure of core support due to latent defects: $2 \times 10^{-7}$ per r.y.
- Failure of major closures leading to failure of the core support: $5 \times 10^{-8}$ per r.y.

Probability of delayed failure of containment within a period 0-10 hours, 0.3 per demand, and within a period 10-100 hours, 0.6 per demand.

in the latter case the maximum acceptable frequency of failure for any one of $m$ closures whose failure could lead to this sequential damage would be $5/m \times 10^{-8}$ per r.y.

6.6 SUMMARY OF RELIABILITIES REQUIRED FROM REACTOR PROTECTION SYSTEMS AND MAJOR STRUCTURES

The results of the estimates obtained in the earlier parts of the section are summarized in Table 6.3.
Table 6.3 Summary of maximum acceptable failure rates for principal systems and features relevant to reactor safety, in certain specific conditions

<table>
<thead>
<tr>
<th>Serial No.</th>
<th>Function</th>
<th>LMFBR</th>
<th>GCFR</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>Reactor shutdown</td>
<td>$1.8 \times 10^{-6}$ per demand</td>
<td>$9.0 \times 10^{-6}$ (upflow) per demand</td>
</tr>
<tr>
<td>2</td>
<td>Post-trip core flow</td>
<td>$1.6 \times 10^{-6}$ per demand</td>
<td>$0.7 \times 10^{-6}$ per demand</td>
</tr>
<tr>
<td>3</td>
<td>Failure of core support due to internal defect</td>
<td>$2 \times 10^{-6}$ per r.y.</td>
<td>$2 \times 10^{-7}$ per r.y.</td>
</tr>
<tr>
<td>4</td>
<td>Combined failure of primary tank and leak jacket</td>
<td>$4 \times 10^{-8}$ per r.y.</td>
<td>N/A</td>
</tr>
<tr>
<td>5</td>
<td>Complete failure of PCRV</td>
<td>N/A</td>
<td>$1 \times 10^{-7}$ per r.y.</td>
</tr>
<tr>
<td>6</td>
<td>Failure of major PCRV closures capable of causing core support failure</td>
<td>N/A</td>
<td>$5 \times 10^{-8}$ per r.y.</td>
</tr>
</tbody>
</table>

*Second significant figure is only inserted to demonstrate that differences exist.

The results in Serials 1 and 2 suggest that in practice it might be preferable in the case of LMFBR to allocate a higher proportion of the total acceptable risk to failures of the shut-down system, easing the requirement by, say, a factor of 1.5 and increasing the severity of the demand for post-trip core flow by a factor of 2. In the case of GCFR a better balance in the design might be obtained by the reverse procedure.

7. SUBSTANTIATION OF QUANTITATIVE DATA USED IN RELIABILITY ANALYSES

7.1 Substantiation of data from the results of reactor operation

Many of the numerical values used in the analysis described in Section 6 are based on experience with other reactor systems and related plant. It is therefore useful to review the extent to which further experience, obtained during the period before commercial fast breeder reactors are likely to be built in significant numbers, is likely to be of assistance in substantiating the data used in reliability analyses of the breeder reactors.

Considering firstly the frequencies assumed for primary faults. In the case of GCFR the continued flow of information from the operation of normal GCRs should be of value, particularly as there will be a higher proportion of such reactors in operation which have a closer resemblance to the current GCFR designs, e.g., features such as the PCRV, once-through boilers and advanced steam conditions are common to the AGR and HTRs already built or under construction and there are some 10 thermal GCRs in operation which use steam-driven circulators. In the case of LMFBR there will not be the same volume of experience prior to committing the first commercial plant as the number of prototype and demonstration plants projected is small. However a useful amount of data should be forthcoming concerning the frequency of subassembly faults, which is one of the most important factors so far as the reliability required from the protection systems is concerned, as the numbers of subassemblies used will be large.
There is also sufficient similarity in the design requirements and environmental conditions between GCFR and the thermal GCRs for the accumulating experience with reactor trip systems to be of value in refining the estimates for the failure rates which would be encountered in GCFR. In the case of LMFBR only the experience in other LMFBRs will be relevant.

Several elements of a typical emergency core cooling system, notably the diesel alternators and the automation of operating procedures, are common to all types of reactors. In this respect therefore the LMFBR should not be at a disadvantage. Operating experience with LWRs should provide useful data concerning the reliability of secondary containment systems, in relation to the retention of leak tightness during service, but clearly little operational experience relevant to the reliability of core catchers should be obtained.

Overall, therefore, operating experience with other reactor systems should make a useful contribution to the refinement of the data on which the quantitative evaluation of the safety of the fast breeder reactors is based and should help to confirm that the reliabilities required from reactor protection systems are attainable with current design practices, particularly in the case of the GCFR.

7.2 Substantiation of the weighting factors for changes in conditions of release

The weighting factors listed in Note 8 to Table 6.2 are necessarily of a somewhat speculative nature. However, they embody the following considerations:

(a) The factor of 10 between core meltdown, or FCI explosion, and partial vapourization of the core reflects the increased toxicity of the more important refractory fission product isotopes, notably Sr-90, and the plutonium isotopes as compared with I-131, together with the relative amounts (as measured in curies) of each which is present in the core at equilibrium.

(b) The smaller factors assumed for LMFBR in all cases where the release is delayed reflect the greater potential removal of isotopes such as I-131 and Cs-137, due to the presence of sodium, as compared with the plateout and condensation processes which could occur in the GCFR if release is delayed. In cases where whole body effects are important the values for LMFBR may be over-optimistic, as the noble gases will not be absorbed.

7.3 Substantiation of the probabilities assumed for the occurrence of unexpectedly violent nuclear and FCI explosions

Although in recent years the trend of results obtained from the theoretical calculation of explosive yields has been downwards the increasing complexity of the methods and the number of input parameters gives cause for concern about their accuracy. As an illustration of the cumulative effect of small errors in the individual parameters it is instructive to consider a hypothetical example in which the solution depends on 100 separate parameters, all of which appear to the first power and each of which is normally distributed. If the coefficient of variation for each of the parameters were only 5 percent we would expect to find that in one case out of a hundred the result would be more than double that which would be obtained from a calculation based on the mean values of each parameter.

As discussed in Section 6, above, there are other factors in the case of the GCFRs which reduce the probability of unexpectedly violent explosions to a substantial extent, which have been quantified by introducing further factors of 10 and 100 for the upward and downward flow cores respectively.

It follows that if a relatively simple phenomenon, of an inherent nature, such as the fission product gas effect (Ref. 27) can be demonstrated to be effective in
limiting the explosive yield to the order of 10^14 joules, in all cases, a much less conservative view could be taken of the risk of explosions of sufficient violence to cause immediate failure of the primary and secondary containment, followed by a large and immediate release of vapourized core material. If this were the case there could be a substantial reduction in the reliability required from the LMFBR shut-down system and in the need to trip the reactor in response to single subassembly faults. The results of the analyses for the GCFR are less sensitive to this parameter.

7.4 Substantiation of other data used in the analyses

In addition to the items of data discussed above the position concerning substantiation of the other data used in the analyses may be summarized as follows:

(a) Target frequency for large releases

Confirmation of the values used can come only from the regulatory bodies concerned.

(b) Frequency of primary faults

With the exception of those involving structural failures, which are discussed below, there is sufficient evidence to show that the values listed should be attainable in practice. Using design practices already established for thermal reactor stations the departures from the estimated values should not exceed a factor three in either direction.

(c) Failure rates for containment and core catchers

The estimated values are believed to be conservative; as indicated in Section 6, the performance of the secondary containment is not nearly so important as that of the core catcher, in severe fault conditions; the estimated failure rate for the core catcher is so high that it can scarcely be in error by a factor of more than 3, on the optimistic side, but it could be over-conservative by a factor of at least 10.

(d) Frequency of minor structural failures

In compiling the estimates for the frequency of primary faults it has not been necessary to attribute high reliability to features such as minor penetration closures in GCFR, or to external ducts in a loop-type LMFBR, where failure in a reactor of sound basic design would not prevent operation of the shut-down system and continuity of core flow. In general the failure rates assumed are at a level where economic considerations alone are a strong incentive to ensure that they are met during the lifetime of the plant.

7.5 Common mode faults

No attempt has been made to consider this type of fault extensively in the analyses, as this can only be done effectively in terms of specific designs. For example it has been assumed that adequate precautions would be taken against damage by external agencies (missiles, floods, etc.) and self-generated damaging agents (failure of rotating plant, fire) based on conservative estimates of the frequency of such events.

However, the problem has been recognized implicitly by assuming only modest reliabilities (failure rates not less than order 10^-4 per year, as in Ref. 4) for individual protection systems, thus reducing the task of achieving the suggested reliability to a feasible level, as discussed in more detail in the following section.

8. FEASIBILITY OF ATTAINING NECESSARY RELIABILITY IN THE REACTOR PROTECTION SYSTEMS AND STRUCTURES

8.1 Feasibility of meeting requirements for reactor shut-down system
8.1.1 LMFBR

In the illustrative example of the previous Section it was shown that the maximum acceptable failure rate was $2 \times 10^{-5}$ failures per demand. The problems of attaining high reliability in shut-down systems have been analysed quantitatively in Refs. 13 and 14, and elsewhere. There is a general belief that for a single system it is unlikely that a failure rate better than about 10 failures per demand can be maintained in practice, due mainly to the effects of potential common mode faults.

Thus in the LMFBR it would be necessary, in this example, to provide two separate shut-down systems sufficiently diverse in character to reduce the risk of common mode faults affecting both by a factor of 50, as compared with the same risk in a single system. To achieve this it would probably be necessary to provide completely independent sensors of a diverse nature and in diverse locations, independent and diverse units for processing the sensor signals, including the guard lines themselves, and diversity in the absorber elements.

In practice some difficulties may arise in meeting these requirements, for example:

(i) The most frequent fault against which trip protection is required is the subassembly fault. It is still uncertain whether adequate sensing devices of a whole-core nature can be devised to give protection against this type of fault (e.g., see Ref. 15). If this is not possible it would seem equally difficult to provide two sets of diverse types of sensor on each subassembly, each in a two-out-of-three arrangement.

(ii) Although investigations have been made into the possibility of providing alternatives to the rod type of absorber commonly used in LMFBRs, little progress has been made as yet (e.g., see Ref. 16). Alternative concepts such as the injection of "poisons" into the sodium (see Ref. 17) would seem to be too slow acting in the more severe fault conditions and could have a very adverse effect on availability. In this connection it should be noted that the possibility of "sodium frost" deposition introduces a potential common mode fault which could affect all rods, unless they are released below the level of the sodium surface.

Overall, therefore, there must be some doubt as to the feasibility of attaining sufficient reliability in the shut-down systems of the LMFBR to meet the target used for illustrative purposes in Section 6.2. It would be difficult to meet a more severe target. Thus the relief, by a factor 1.5, indicated in Section 6.6 would help.

8.1.2 GCFR

It follows from the discussion in the previous Section that it is doubtful whether the maximum acceptable failure rates ($1 \times 10^{-5}$ for both upward core flow and downward core flow versions) could be attained by a single system. The use of two systems, if completely diverse and independent, should decrease the risk of failure to a level substantially less than that indicated as necessary by the illustrative example of Section 6.2.

Experience with thermal GCRs demonstrates that completely diverse systems, employing boron steel balls, for example, instead of rods can be engineered which would have a sufficiently rapid insertion time to meet GCFR requirements. Moreover such systems are much less likely to be made ineffective by distortion or displacement of the subassemblies.

Furthermore, as individual subassembly faults do not present a requirement for rapid shutdown in the GCFR, all the trip sensing devices can be designed on a whole-core basis, leading to greater simplicity and inherent reliability.

Overall, therefore, there is no doubt that sufficient reliability could be attained in the shut-down system of a GCFR to meet a target at least a factor of 10 more severe than that used in the illustrative example of Section 6.2.
8.2 Feasibility of meeting requirements for continuity of core flow

8.2.1 LMFBR

Quantitative analyses of the reliability of core flow in typical loop (SNR) and pool (PFR) type LMFBRs have been reported in Ref. 11 and Ref. 9 respectively.

In the former case it is shown that the failure rate of the separate emergency system, provided to take over if the main loops become ineffective, is between $10^{-5}$ and $10^{-6}$ per demands, without taking any credit for natural circulation.

In the latter case the maximum failure rate is higher (1 in $10^{-4}$ demands) but this appears to result from the somewhat limited provision for emergency electrical supplies (2 diesel alternators only), which in turn reflects the rather mild requirements specified to the designer. In this latter case, also, no credit was taken for natural circulation and it was estimated that the frequency of failure of the main loops would be about $10^{-2}$ per event which leads to a sudden change in the cooling requirements.

In the illustrative example of Section 6.3, above, it was estimated that the maximum acceptable frequency of failure would be $2 \times 10^{-6}$ per demand for continuity of cooling, after a major change in operating conditions. Thus from the above discussion of the results obtained for firm designs in Refs. 10 and 11, it can be concluded that there should be no difficulty in meeting the target proposed in the example of Section 6.3, above and it should be possible to meet one more severe by a factor of at least 10, if credit can be taken for continued operation of the main loops and for natural circulation for, say, 30 minutes.

8.2.2 GCFR

A quantitative analysis of the reliability of core flow in a typical GCFR with downward flow core and steam-driven circulators has been reported in Ref. 8, and a similar study for an HTR design with downward flow but a more complex electrical-steam drive for the circulators has been reported in Ref. 9. The first of these studies shows that the failure rate for loss of continuity in flow following a major change in operating conditions can be made as low as $10^{-6}$ per demand and the second shows that a rate of $10^{-8}$ per demand is attainable, so far as random faults are concerned. Care in the detailed design would be necessary to ensure that comparable reliability in the presence of the common mode faults was obtained but it should be feasible. It is understood (Ref. 18) that comparable results to those of Ref. 8 have been obtained in the analyses for a GCFR with upward flow core and conventional electric drives to the circulators but with separate stand-by, auxiliary cooling loops.

In the illustrative example of Section 6.3, above, it was estimated that the maximum acceptable frequency of failure per demand for continuity of cooling after a major change in operating conditions would be about $10^{-6}$. Thus it should be feasible to meet this requirement in practical designs and it may be possible to meet targets more severe by factors of at least 10 than those postulated for illustrative purposes in Section 6, above.

8.3 Feasibility of meeting requirements for the core catcher and containment systems

The reliability assumed for the core catcher in the illustrative example of Section 6.3 is so low (probability of failure in periods of 0 to 10 and 10-100 hours, 0.1 and 0.8 respectively) that it should not be difficult to achieve. An improvement in core catcher reliability would lead to a marked easing of the reliability required from the other protection systems, particularly in the case of GCFR, where the improvement is almost pro rata.

The reliability of the secondary containment system, in the sense of freedom from defects arising during normal operation, is not a major factor in severe fault conditions, so far as direct control of activity releases is concerned, although freedom from gross leakage (greater than 10 percent per day) could be important.
in GCFR designs where a high back-pressure in the containment is necessary to ensure adequate emergency cooling. In both systems a high standard of leak-tightness may be necessary during normal operation in order to control daily releases or the activity released in minor faults, but this is a separate issue in which a high standard of leak-tightness at low differential pressures is the critical characteristic.

8.4 Feasibility of meeting requirements for structures

Methods for the quantitative assessment of structural failure rates have been under development for over a decade (e.g., see Ref. 19) and considerable progress has been made, particularly in the aerospace industry. A few reports have been published concerning the direct application of these methods to reactor structures, notably Ref. 20, which describes the extensive use of these techniques in relation to the design of the Nerva reactor, and Refs. 21 and 22 which demonstrate their application to prestressed concrete containment structures and reactor vessels, respectively. A particularly useful application of these techniques should be to relate the factors of safety used in conventional structural design procedures to the probability of failure. However, there are no published results which can be applied directly at the present time to this comparative study, although in due course the position should improve.

An alternative approach is to consider the extent to which good design, initial inspection and testing of a structure, supplemented by regular surveillance, and possibly by re-testing in service, can provide adequate assurance that the risk of failure during operation is adequately low. For want of any better method this approach is necessarily used for both steel and concrete reactor vessels and has been of considerable value in defining the inspection facilities which must be incorporated into a design in order to make adequate surveillance possible. The same concept can be applied to critical internal structures such as the core support. In general the extent of in-service inspection can be reduced as the degree of redundancy in a structure is increased but in critical cases it is doubtful whether it can be dispensed with entirely. In this context the GCFR should have a considerable advantage over the LMFBR, as operating experience with large GCRs has demonstrated that detailed inspection of the reactor internals by remote means is possible without incurring excessive outage time, whereas with an opaque coolant which is solid at temperatures up to 98°C it is difficult to visualize how adequate inspection of, say, the core support or the pump outlet ducts could be carried out without removing all the fuel and draining the reactor tank; the adverse effect of draining down on availability would act as a disincentive to frequent inspection.

Considering the maximum acceptable frequencies of failure derived in the illustration example of Sec. 6.5 the following conclusions can be drawn:

(a) LMFBR

The primary tank and leak jacket should be sufficiently independent of one another, other than in exceptional circumstances such as seismic disturbances more severe than those considered by the designer, that the acceptable failure rate (4x10^-8 per r.y.) should be readily attainable. The maximum acceptable failure rate is also important as a means of defining the most severe earthquake which should be considered in the design, as the probability of this event must be related to the maximum acceptable failure rate for the most critical structures.

The core support structure is in a hostile environment due to the effects of vibration, thermal stress, irradiation and the coolant itself. The uncertainties in relation to these effects on the properties of the material and the applied loads will make quantitative analyses of the reliability of these structures particularly difficult and will therefore accentuate the need for in-service surveillance.
The provision of redundancy, in those features of the structure which are essential to ensure that the core can be shut down and continuity of cooling maintained, is an alternative solution which would reduce the need for in-service inspection. It may not be necessary to extend this redundancy to that part of the structure which serves as a barrier between the inlet and outlet sides of the core; this will depend on the details of the design in question.

(b) GCFR

The maximum acceptable frequency of complete failure of the PCRV (10^-7 per r.y.) is a factor of 10 lower than that which can be attributed with confidence to a large steel reactor vessel but it is not out of keeping with the commonly held view in several countries that PCRVs can provide a substantially greater degree of safety than steel vessels. As discussed in Ref. 23, for example, it should be easier to achieve a given effectiveness of in-service surveillance for PCRVs than would be the case for steel vessels.

It is now almost a universal practice to provide two independent closures, or one closure and a restrictor, or two independent hold-downs, for the major penetrations of PCRVs. Provided that the pair of devices can be demonstrated to be independent the required reliability would be readily attainable. As an alternative solution the core support structure could be made sufficiently strong to withstand the aerodynamic load due to the most severe depressurization accident which is conceivable.

The maximum acceptable failure rate for the core support structure (for failures due to latent defects), together with the hostile environment due to vibration, thermal stress and irradiation, implies either that the structure must have a high degree of redundancy together with some limited facilities for in-service inspection of more extensive in-service surveillance facilities. Either of these solutions would appear to be feasible in practice.

Overall, therefore, in the case of the GCFR it should not be unduly difficult to meet the requirements for structural reliability which have been derived in the illustrative example of Section 6.5 but in the case of the LMFBR, which at first sight should present fewer problems, it may prove more difficult in practice to demonstrate adequate reliability without incurring excessive outage time for inspection during service.

The reliabilities required from those structures whose failure would not interfere with the reactor shut-down system or would not prevent continuity of core cooling are relatively low (e.g., maximum acceptable frequency of failure of minor penetrations in PCRV is 10^-3 per r.y.) and should not be difficult to attain. Moreover the maximum acceptable failure rates dictated by economic considerations are such that, in general, potential faults do not have a major influence on the reliability required from the protection systems.

9. EFFECT OF MAJOR CHANGES IN INPUT DATA ON THE RESULTS OF THE RELIABILITY ANALYSES

9.1 Changes in quantitative safety target

Assuming that the values of the other parameters used in the illustrative examples of Section 6, above, remain the same the following conclusions can be drawn:

(a) LMFBR

As discussed in Section 8.1.1 (a), above, there is some doubt as to the feasibility of attaining sufficient reliability in the reactor shut-down system to meet the safety target assumed in Section 6, with current techniques. Thus further development of the shut-down system, in terms of sensors to detect sub-assembly faults and in diversity of absorber, would be necessary to meet a more severe target. Conversely a reduction in severity would ease the situation.
As discussed in Section 8.2.1, above, the feasibility of ensuring sufficient continuity of core flow to meet the target assumed in Section 6. is not in doubt and an increase in severity of the target by a factor of at least 10 could be accommodated.

To demonstrate that the core support has adequate reliability to meet the present target may necessitate significant reactor outage time to facilitate inspection. In principle a more severe safety target would imply more frequent inspection, in addition the problem of demonstrating that the probability of a "greater than design" earthquake was sufficiently low could become increasingly difficult and perhaps impossible.

(b) GCFR

In this case, as discussed in Section 8.1.2, the shut-down requirements and established techniques are such that an increase by a factor of at least 10 in the severity of the target could readily be accommodated.

It follows from the discussion in Section 8.2.2 that so far as continuity of core flow is concerned it should be possible to accommodate an increase in the severity of the target by a factor of 10, but the effect of common mode faults would make a further increase in severity difficult to meet.

The most limiting factor in the case of the GCFR, in terms of demonstrating by quantitative arguments that the reactor can meet a specific safety target, is the reliability of the PCRV itself. It is likely to be several years before it can be demonstrated satisfactorily that a given vessel would have a frequency of failure of less than $10^{-7}$ per r.y.

Overall, therefore, quantitative analyses, based on available techniques and on the data listed in Tables 6.1 and 6.2 could not be used to justify an increase of order 100 in the safety target for either system at the present time. However, for the GCFR, apart from the problem of demonstrating PCRV integrity, a factor of 10 could be accommodated by a feasible design.

9.2 Changes in the frequency of primary faults

It follows from Eq. A.II.1 that an increase in the frequency of all primary faults by a factor of, say, 10 would have the same effect on the reliability required from protection systems as an increase in the severity of the safety target by the same factor. Thus it follows from the discussion in Section 9.1 that, if the other parameters remain unchanged, an under-estimate by a factor of 10 in the frequencies assumed for each of the primary faults listed in Table 6.1 could be tolerated, except possibly the frequency of subassembly faults in LMFBR. Larger changes in the frequency of some primary faults could be tolerated.

9.3 Changes in the probability of certain sequential effects

By reference to the detailed calculations sheets for the illustrative examples in Section 6 it can be shown that in the case of LMFBR the higher reliability required from the reactor shut-down system is due mainly to the combination of the higher frequency of faults requiring trip action (an increase from 0.2 to 1.2 per r.y. as compared with GCFR) and the higher probability assumed for unexpectedly violent explosions, capable of causing immediate failure of both the primary and secondary containments. A change in either of these parameters would lead to an almost pro rata change in the reliability required from the shut-down system. In the case of the reliability required from the continuity of core cooling the FCI explosion sequences do not have a dominant effect, core meltdown being more important.

In the case of the GCFR the reliability required from both the reactor shut-down system and the continuity of core cooling is mainly dependent on those sequences in which a delayed release of activity occurs following a core meltdown without
any core vaporization.

Thus in the case of LMFBR a substantial improvement in safety, for a given degree of reliability in the shut-down and cooling systems, would be demonstrated by a reduction in the probability of exceptionally violent nuclear explosions, and some improvement would be obtained if a higher reliability could be assumed for the core catcher. In the GCFR, the converse situation exists. On the one hand an improvement in the reliability of the core catcher, or in the weighting factors, would give an almost pro rata improvement in safety but a reduction in the probability of exceptionally violent nuclear explosions would have little effect. On the other hand a 10-fold increase in the latter probability would only decrease the safety of the reactor by a factor of about 2 in the upward core flow case; in the downward core flow case the effect would be barely significant.

The reliability required from the major reactor structures has been determined from the values assumed for other parameters which affect the release of activity. In the case of the LMFBR the situation is again dominated by the probability assumed for an exceptionally violent nuclear explosion, whereas in the GCFR the high probability of failure for the core catcher in these conditions, together with the larger weighting factor assumed for the delayed releases are the dominant factors.

Overall, therefore, to demonstrate that the safety of fast reactors is higher than is indicated by the illustrative example in Section 6. requires radically different approaches for the two systems. Thus:

(a) In the case of LMFBR it would be necessary to show that the risk of exceptionally violent nuclear explosions is smaller than has been assumed.

(b) In the case of the GCFR it would be necessary to show that either a higher reliability could be obtained from the core catcher or that the weighting factor for delayed releases were lower than assumed in Section 6.5.

Hence further work to demonstrate that inherent properties such as the fission product gas effect would limit the violence of nuclear explosions is of considerable importance to further development of LMFBR but is of less importance to GCFR.

10. EFFECT OF ECONOMIC FACTORS ON SAFETY OF FAST BREEDER REACTORS

10.1 The significance of doubling time

At the present time LMFBR and GCFR represent alternative means of both conserving natural resource of fissile material and of generating power. The most recent estimates of capital costs for commercial versions of the two systems (Ref. 18 and 24) that would embody design features assumed in the illustrative example of Section 6. above, suggest that there would be no significant differences in their costs and that both would be slightly more expensive than thermal reactors of the same size; when full credit has been taken for the reduction in cost resulting from large-scale production of the breeders. Given a sufficiently good breeding performance, the lower fuel cost could offset the difference in capital cost but the margin is small. Thus neither system is likely to be highly competitive with thermal systems simply as generators of power.

It follows that in order to be economically viable, these systems must have good breeding properties (i.e., short doubling times). In the case of GCFR the designs which have been used as a basis for this analysis should give doubling times of about 10 years whereas the corresponding LMFBR designs have doubling times of about 20 years.

The only foreseeable way in which the doubling time for LMFBR could be halved would be to double the rating of the core. In general it is to be expected that an increase in rating would be detrimental to safety, due to the increased rate
of rise of fuel temperatures in fault conditions. Moreover in a fast reactor, if nuclear explosions are a significant factor, the potential explosive yield would be increased. In addition, the design of an effective core catcher would become more difficult if specific power is increased substantially.

In the case of the LMFBR it seems likely that, on qualitative grounds, the main safety problem arising from a substantial increase in rating would be an aggravation of the difficulties in providing sufficient reliability in the reactor shut-down system, as the time available for sensing the fault and for absorber insertion would be substantially reduced. To quantify these effects precisely would require detailed analysis of specific designs. However, as discussed in Section 9, above, it is the risk of major errors in the estimates of nuclear explosion yields in LMFBR which has a predominant influence on the reliability required from the reactor trip system. Thus, until greater accuracy in these estimates is assumed, greater difficulties in designing an effective trip system would have a marked effect on the level of safety which can be demonstrated for the system.

10.2 Effect of changes in assumptions concerning design features of commercial breeder reactors

In the illustrative example of Section 6 there are some implicit assumptions concerning the effect of economy on safety, e.g., in Serials 4 and 5 of Table 6.2 the assumed probabilities for the sequences "Nuclear (or FCI) explosion plus immediate failure of primary containment plus immediate failure of secondary containment" are based on the premise that in the case of LMFBR economic considerations would deter the designer from providing a primary containment or secondary containment of such strength that no conceivable nuclear or FCI explosion would cause failure; in Serials 8 and 10 it is assumed in the case of GCFR that any relief valve discharge would be to the helium storage tank. It is believed that the assumptions which have been made are consistent with those used in the development of the designs for commercial plants on which the cost estimates referred to above are based.

A feature such as a pre-stressed concrete leak jacket for LMFBR (Ref. 28) would presumably add significantly to the capital cost; if the intermediate cooling loops could be dispensed with in LMFBR there would be a substantial reduction in capital cost, but the conclusions drawn from the illustrative example in Section 6, above, would need revision.

10.3 Effect of availability on safety

In assessing the relative economic merits of alternative reactor systems it is necessary to consider the availability which is likely to be obtained from reactors of each type.

In this respect GCFR would appear to have an advantage over LMFBR due partly to the better facilities for internal inspection during service, as discussed in Section 9.1, above, and partly to the more extensive operational experience in support of component development which could be provided by a substantial and successful program of commercial HTRs. In that situation there would be some pressure on the LMFBR designers to reduce capital or fuel costs, in order to demonstrate advantages in this respect, which in turn could reflect on safety. However, at a later stage, as operating experience with large LMFBRs is accumulated, this difference should diminish.

11. CONCLUSIONS

11.1 Standard of safety currently demonstrable

From the quantitative analysis described in this paper it can be concluded that in general both the LMFBR and GCFR can be designed so that the overall hazard to the public from one of these reactors would be no greater than that
presented by an LWR of the same size. The only caveats to this general conclusion are that:

(a) In the case of LMFBR there is some doubt as to the feasibility of providing sufficient reliability in the trip protection against individual subassembly faults, with currently available techniques. In addition the practical problems of carrying out sufficient in-service inspection of the core support to ensure adequate reliability could lead to excessively long outages.

(b) In the case of GCFR adequate techniques for evaluating the probability of complete failure of the PCRV are not yet available.

Subject to these uncertainties it should be possible to improve the standard of safety of both systems by a factor of 10 without requiring unrealistically high reliabilities from the reactor protection systems, using currently available design concepts and techniques. However, the changes required would add to the cost of both systems.

11.2 Possible means of demonstrating a higher standard of safety in future

If, in the case of the LMFBR, it could be shown that the probability of unexpectedly violent nuclear explosions is lower than assumed in this analysis, the standard of safety demonstrable would increase almost pro rata. In addition the need for high reliability of tripping in response to subassembly faults would be decreased. The methods described in this paper could be used to determine the accuracy required from the calculations of explosive yield and the resulting effects on reactor structures.

If, in the case of the GCFR, it could be shown that over-conservative values have been used for the weighting factors applied in estimating the effects of delayed releases, an almost pro rata increase in the standard of safety could be demonstrated. Alternatively, the development of a core catcher with higher reliability than has been assumed in the analysis would have a similar effect.

11.3 Effect of economic factors on safety

As it is unlikely that LMFBR will be able to show any significant cost or safety advantage over GCFR there will be a strong economic incentive to attain comparable doubling times. To achieve this would require an increase in the rating of LMFBR by a factor of about two. This increase would lead to move severe temperature transients in fault conditions which in turn would aggravate the problems of providing sufficient reliability in the reactor protection systems. However, the magnitude of this effect could only be quantified by detailed analysis of specific designs.

Initially, if there is a substantial and successful program of commercial HTRs in the U.S. or elsewhere, the GCFR is likely to offer firmer prospects of good availability than the LMFBR; this situation would also increase the pressure to obtain lower generating costs in the LMFBR. This pressure may reflect adversely on safety until LMFBR is able to demonstrate that in practice comparable availability can be attained.

11.4 Relative merits of different configurations of LMFBR and GCFR

This preliminary quantitative analysis does not show any significant differences in overall safety as between, on the one hand, the loop and the pool-type LMFBR and between the upward and downward core flow GCFR designs, on the other.
ACKNOWLEDGMENT

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REFERENCES


REFERENCES (CONT.)


APPENDIX I

DERIVATION OF A QUANTITATIVE SAFETY TARGET FOR FAST REACTORS

A safety target can be established which is closely comparable with that currently in use in the U.S. for LWR, as described in Ref. 4. The method of deriving this target is as follows:

The U.S. policy for LWRs, as stated in Ref. 4, is that "the safety objective will require that there be no greater than one chance per million per year for an individual plant of an accident with potential consequences greater than the Part-100 Guidelines."

In the case of the LWR there are three principal types of fault sequence events which could lead to very large releases to the atmosphere (say 10 per cent or more of the gaseous and volatile fission products). These sequences, and the relative weight attached to each, in terms of the size of release are:
(i) Disruptive failure of the reactor pressure vessel, due to a latent defect, and destruction of the engineered safeguards. Weighting factor for release condition - 1.0

(ii) As (i) but failure of vessel due to a failure to scram following a fault condition requiring a rapid trip. Weighting factor - 1.0

(iii) Rupture of primary circuit followed by meltdown of core due to failure of ECCS, leading to deferred failure (of the order of 10 hours) of the secondary containment. Weighting factor - 0.1

If the probabilities of these three sequences are $s_1$, $s_2$, and $s_3$ per r.y., the total expectation of damage per r.y. is $k(s_1 + s_2 + 0.1s_3) = E$, where $E$ can be measured in terms of the total number of additional cancer cases per year due to the operation of the reactor and $k$ is the appropriate constant.

From the studies carried out in the U.S., U.K. and FRG it can be inferred (Ref. 25) that $s_1$ lies between $10^{-6}$ and $10^{-7}$ per r.y. Thus we can assume without excessive error that $s_1 = 3 \times 10^{-7}$; this is consistent with the assumption that the contributions to $E$ which are made by each of the sequences described above are approximately equal, when the weighting factors for conditions of release have been taken into account. Consequently we have $k \times 10^{-6} = E$.

For a fast reactor system, which is to be located on sites with similar characteristics, it is assumed that all the fault sequences can be identified which could lead to a large release of activity; the annual probabilities of these sequences are denoted by $s_1$, $s_2$, $s_3$, ... It is further assumed that from consideration of the conditions resulting from a particular fault sequence, 'i', a weighting function, $n_i$, can be derived which expresses the "damage potential" of the release due to sequence 'i'; $n_i$ would have the same units as $E$.

Thus the total expectation of damage from the reactor would be $\sum s_i n_i$, where the summation extends over all possible sequences. Consequently in order to present no greater hazard to the public than an LWR of the same size on the same site the relationship

$$\sum s_i n_i \leq E$$

must be satisfied. This can be re-written in this form:

$$\sum s_i \frac{n_i}{k} 10^6 \leq 1$$

$$\left(\frac{n_i}{k}\right)$$ now represents a dimensionless weighting factor which has the value 1 for releases of the type resulting from a disruptive pressure vessel failure, accompanied by failure of the engineered safeguards, in an LWR. It is more convenient therefore to write, in the limit,

$$\sum s_i w_i = 1 \quad \text{Eq. A.1.1}$$

where $w_i$ has the value $10^6$ for the LWR case described.

**APPENDIX II**

**DETERMINATION OF MAXIMUM ACCEPTABLE FAILURE RATES**

**FOR REACTOR PROTECTION SYSTEMS AND CRITICAL REACTOR STRUCTURES**

Suppose that we require to determine the maximum acceptable failure rates of the protection systems of a reactor, or the major structures, given a safety target of the form described in Appendix I together with the following data for the reactor and its associated power plant:
1. Number of different protection systems or structures whose failure could lead to core meltdown, with or without explosion. This is denoted by \( N_p \).

2. Annual frequency of primary faults which require a given protection system, \( x \), to function in order to prevent core meltdown. This is denoted by \( n_x \).

3. Probabilities of events (1), (2), (3), which form a particular sequence, \( j \), leading to core meltdown and release of activity to the atmosphere after a primary fault of a type requiring protection system \( x \) to function, and failure of system \( x \). These are denoted by \( p(j_1), p(j_2), p(j_3), \ldots \).

4. The weighting factor, of the type described in Appendix I, to be applied to allow for the conditions of release resulting from sequence \( j \). This is denoted by \( w_j \).

In general we are concerned only with failure of the reactor shutdown system, failure to maintain continuity of coolant flow after shutdown and failure of certain critical structures. Previous experience (Ref. 8) indicates that initially it is advisable to assume that each of these groups should make an equal contribution to the total risk. I.e., \( N_p = 3 \), although this assumption can be varied later, if necessary.

Thus, using a safety target derived as in Appendix I, we can write

\[
\frac{n_x}{f_x} \sum p(j_1)p(j_2)p(j_3) \ldots w_j = 0.3
\]

where the summation extends over all foreseeable sequences resulting from primary faults which require system \( x \) to function.

Hence \( f_x \) can be determined.

In the case of the reactor structures it is necessary to identify those whose failure would prevent either, or both the reactor protection systems from working and to further sub-divide the target allocation between them, initially on an equal basis. Those structures whose failure does not prevent the reactor protection system from functioning can be treated in the same manner as other primary faults. Proceeding in this way, we have, in this case,

\[
f_z \sum p(j_1)p(j_2)p(j_3) \ldots w_j = \frac{0.33}{c}
\]

where \( f_z \) is the maximum acceptable failure rate for structure, \( z \), and \( c \) is the number of critical structures to be considered. Typically \( c \) is likely to be about 3 or 4 for a GCFR but smaller for LMFBR.

A typical set of sequences, for the case of GCFR (upward flow case) with failure to trip, are shown in Fig. A.11.1.

It is assumed in this approach that each step in the various sequences can be regarded as having a "go--no go" characteristic. In practice at most steps there is a probability distribution which defines the response of the system. However, to include these effects would increase greatly the complexity of the calculations and at the present time the accuracy of the available data does not justify this additional elaboration of the analysis. Values for parameters such as the weighting factor, \( w_j \), for release conditions have therefore been selected which are believed to be reasonably representative of the mean.
### GCFR Upward Flow Core, Failure to Trip

<table>
<thead>
<tr>
<th>Core</th>
<th>Primary Circuit</th>
<th>Core Catcher</th>
<th>Secondary Containment</th>
<th>$n_d(j,i)$</th>
<th>Weighting Factor</th>
<th>Contribution to Risk</th>
</tr>
</thead>
<tbody>
<tr>
<td>Nuc. Ex.</td>
<td>Imm. F.</td>
<td>BY-PASS</td>
<td>Imm. F.</td>
<td>$10^{-3}$</td>
<td>$10^7$</td>
<td>$10^4$</td>
</tr>
<tr>
<td>Nuc. Ex.</td>
<td>Imm. F.</td>
<td>BY-PASS</td>
<td>FAULT $10^{-3}$</td>
<td>$10^{-5}$</td>
<td>$10^7$</td>
<td>$10^2$</td>
</tr>
<tr>
<td>Nuc. Ex.</td>
<td>Imm. F.</td>
<td>10 Hr F.</td>
<td>1 Hr F.</td>
<td>$10^{-3}$</td>
<td>$3x10^6$</td>
<td>$3x10^3$</td>
</tr>
<tr>
<td>Nuc. Ex.</td>
<td>Imm. F.</td>
<td>100 Hr 0.8</td>
<td>1 Hr F.</td>
<td>$10^{-3}$</td>
<td>$3x10^6$</td>
<td>$9x10^3$</td>
</tr>
<tr>
<td>Nuc. Ex.</td>
<td>Intact</td>
<td>10 Hr 0.1</td>
<td>1 Hr F.</td>
<td>$3x10^{-3}$</td>
<td>$3x10^6$</td>
<td>$2.4x10^4$</td>
</tr>
<tr>
<td>Nuc. Ex.</td>
<td>Intact</td>
<td>100 Hr 0.8</td>
<td>1 Hr F.</td>
<td>$2.4x10^{-2}$</td>
<td>$10^6$</td>
<td>$2.4x10^4$</td>
</tr>
<tr>
<td>C.M.D.</td>
<td>Intact</td>
<td>10 Hr 0.1</td>
<td>1 Hr F.</td>
<td>$10^{-1}$</td>
<td>$3x10^5$</td>
<td>$3x10^4$</td>
</tr>
<tr>
<td>C.M.D.</td>
<td>Intact</td>
<td>100 Hr 0.8</td>
<td>1 Hr F.</td>
<td>$8x10^{-1}$</td>
<td>$10^5$</td>
<td>$8x10^4$</td>
</tr>
</tbody>
</table>

$\sum = 16.4 \times 10^4$

**Fig. A II.1**
EVALUATION OF THE PROBABILITY OF AN AIRCRAFT CRASH ON A NUCLEAR POWER PLANT

M. CRAVERO - G. LUCNET (E. de France)

1 - INTRODUCTION

The liquid Metal Fast Breeder Reactor SUPER-PHENIX, (electrical power 1200 MW) which will be built at CREYS-MALVILLE in the Rhône valley must follow the guidelines given in France for the safety of this reactor. One of these guidelines is to evaluate the risks in relation with air traffic. Consequently, a study of this problem was begun to estimate the probability of an aircraft crash on the power plant SUPER-PHENIX, particularly on reactor building.

Several studies have been made to assess this probability for various sites and crash circumstances (Ref. 1 to 11).

JOERISSEN and ZUEND (Ref. 1) estimated the probability and the consequences of an aircraft crash on a nuclear power plant incorporating a light water reactor. They considered the probability of an aircraft strike, missile penetration through walls and damage of structures and systems important for safety. The probability of an aircraft strike was determined, from the available statistics on the aircraft accidents per flight, assuming about half of these accidents occurred outside the vicinity of an airport, with a distance from an airport at least 20 kilometers, and by considering only commercial aviation (scheduled and non scheduled flights). Furthermore, to facilitate the calculation of the virtual area, it was assumed that the aircraft has an average strike angle of 45 degrees above the horizontal. The result of this study was that in 1980 the average probability for a nuclear power plant to be hit by a civil aircraft, is $10^{-7}$ per year.

CHELAPATI, KENNEDY, WALL, AUGENSTEIN and WAIS (Ref.2, 3, 4, 5), categorized aircrafts according to weight, with a division at 12500 pounds (5680 kg), and classified aircraft accidents with respect to the proximity of the airport (5 miles radius from the airport, 8 kilometers), and estimated the virtual area of the power plant, assuming the trajectory of the aircraft has an angle of 10 degrees above the horizontal, without specifying the direction of impact all around the power plant.

For the four light water reactors, Three Mile Island; Shoreham, Rome Point, Zion Station (Ref. 6, 7, 8, 9), which are located in the immediate vicinity of commercial airports, a study has been done where only aircrafts which are taking off or landing on these airports are considered.

HORNYIK (Ref. 10), in a study for a proposed site near a military aviation training area, introduced traffic density around the airport, and determined the probability of a crash on the site by airplanes landing or approaching at low altitude.

EISENHUT (Ref. 11) studied the probability of an aircraft crash in the vicinity of an airport, as a function of distance from an airport runway and showed for three nuclear plants (Three Mile Island, Shoreham and Rome Point) the probability of an aircraft crash at a nuclear facility is about $5 \times 10^{-7}$ per year.
The present study is concerned with evaluating the probability of an aircraft strike at the 1200 MW LMFBR to be located at CREYS-MALVILLE on the Rhône. Since this nuclear power plant site is not located in the neighbourhood of an important commercial airport, the previous analysis (Ref. 6 to 11) are not appropriate for the present work. The study of JOERISSEN and ZUEND (Ref. 1) is not well adapted to the present case because a) only commercial aviation is considered, b) the strike angle of 45 degrees above the horizontal is a too conservative assumption, and, c) all the accidents occurring outside of the vicinity of an airport cannot be considered as dangerous for a power plant. The analysis of CHELAPATI et al (Ref. 2 to 5) included the following conservatives assumptions: a strike angle of 10 degrees above the horizontal was assumed, and all accidents were considered, even if some were in fact not dangerous for a power plant specially those beyond 5 miles from the airport.

Hence, in the present work three important points are developed for commercial and general aviation (except military operations), with the assumption the power plant is not located in the vicinity of an important commercial airport:

- analysis of aircraft accidents, first for all stages of flight (take-off, cruising, landing) and second, for accidents only during cruising, and examination of the aircraft path after a cruising accident, to define the most probable strike angle with the reactor building.

- calculation of the virtual area of the reactor building taking into account the followings: dimension of the building and of the aircraft involved in the accident, the geographical site and the relative location of other buildings surrounding the reactor building.

- evaluation of air traffic above the considered territory with a division in relation with the aircraft weight, first for commercial aviation (scheduled and non scheduled, international or domestic flights), second for general aviation.

This study done for the period 1980 - 2000 leads us to extrapolate some parameters for this period (growth of air traffic, safety during flight...)

2 - BASIC NOTION DEFINITION

A movement is defined as an operation which is carried out on an airport, either a taking off or a landing, without distinction according to plane type or rendered service (both commercial and general aviation). A flight includes the entire following phases: taking off, cruise and landing, and two flight categories are distinguished:

- domestic flight: its two movements are carried out on a French airport,
- international flight: one of its movements, no more, is carried out on an airport of the French territory.

Flights may correspond either to regular lines or to irregular lines (seasonal or "charters" for instance). All the flights above a territory will constitute the air traffic of the country (both commercial and general air traffics).

Study of accidents allows to establish three categories according to flight phases. Examination of accidents occurring during the phases taking off, or landing, shows us that it is possible to define an "airport area" all around each airport, and we can assume this airport area is a circular zone the radius of which is a few kilometers (Ref. 12). Consequently, the remaining territory is only concerned by occurring during cruise phase accidents. Such a territory discrimination into two areas is only valid for commercial aviation flights. As far as general aviation is concerned, flight distance is generally very short and accidents which are classified as in course accidents may present an impact point in the vicinity of an airport.

Considering a territory, accident probability per flight is defined as fol-
The number of accidents per year on the territory is the probability Pa of each country having different values, which will partially depend on substructure on the ground, but, as it is not possible to determine the variation of this value, we shall consider as reference the international mean value.

The last notion is the virtual area of a building under an aircraft impact. This virtual area is defined as an equivalent on ground surface, which is determined from building external dimensions and reference aircraft dimensions.

Knowing accident probability per flight, Pa, distribution of these accidents versus flight phases, as well as percentage, p, of dangerous for the building accidents, air traffic, N, above a territory which surface is S, and building virtual area Sv, the probability, Pc, of aircraft impact on the considered building is defined as follows:

\[ \text{Pc} = \text{Pa} \times \text{p} \times \text{N} \times \frac{\text{Sv}}{\text{S}} \]

3 - PRINCIPAL PARAMETERS EVALUATION

3.1 - Accident probability per flight

3.1.1 - Commercial aviation

International statistics (Ref. 12, 13, 14, 15, 16) concerning actual aircrafts (tractors or pushers and jets) show that accident probability per flight is ranging about \(0.35 \times 10^{-5}\) with a slight tendency to decrease (plate 1) particularly due to two factors:

- decrease of the number of flights carried out by airscrew tractors or pushers;
- more and more important automatization of aircrafts and on earth substructure (human errors at the present time represent more than the half of accidents).

Such a tendency to decrease can be characterized by \(0.30 \times 10^{-5}\), value which will be adopted for the year 2000.

Accidents detailed analysis (Ref. 12) from records of International Civil Aviation Organization (aviation accidents books), as well as classification in function of flight phases, show that 34.4% of accidents occur in course (period 1962 - 1966) and only 15% of the entire accidents may present a danger for a building: among the remaining in course accidents, certain of them were due to emergency landing, decided during the cruise (4.8%): the aircraft was yet controllable and, in such a circumstance, was able to avoid a building, and 14.6% of accidents are collisions against hilly ground the aircraft being in course.

This value, 0.15, which represents an average computed on eight years (1958 - 1966), will be used as coefficient \(p\), in the probability of an aircraft crash on the considered building. Study of tractors or pushers and jets allows to establish plates 2 and 3, showing that the value of 34.4% for probability of accidents during the cruise is those for airscrew tractors or pushers, and that only 15% of jet accidents occur the aircraft being in course.

Examination of investigation reports on the occured "in flight" accidents shows that:

- generally, engines break off from the machine at the time of impact against the ground,
- parts which break off from the aircraft are wing elements, rudder parts, i.e. low density structures, exception made for the case of an explosion during the flight,
- Aircraft crash is generally vertical when the aircraft loses its ability to be controlled and there are few cases where the aircraft continues to move in gliding flight - that it does then in its initial direction, the broken off from aircraft parts move then in the same direction. Interested by the aircraft fragments surface on the ground is more often a very long rectangle than a circle,

- Impact against the ground is very violent, exception made for the case of a forced landing, and the aircraft then breaks up as the crash speed is generally very important (ranging from 100 to 150 m/s),

- A fire may break out on the ground after aircraft impact, even though the machine was not in fire before crashing.

All these considerations can only be qualitative; however two important points have to be retained:

- **Aircraft crashes**: from 1962 to 1966, for 34 occurred "in course" accidents, two of which are collisions between aircrafts i.e. 36 damaged aircrafts, we find: 19 vertical crashes (spinning dive, nose dive and sometimes back dive of the machine), which represent more than 50% of crashes, and 17 crashes occur with an impact angle on the ground larger than, or equal to, 45° with regard to the horizontal. These values will be used in calculation of the virtual area.

- **Fires**: from 1962 to 1966, for 44 accidents including emergency landings, 26 fires broke out after impact against the earth, when nine machines were in fire at the moment of the impact on the ground.

### 3.1.2 General aviation

The report on the accidents occured in European private aviation during years 1968, 1969, 1970 (Ref. 17) shows that bodily accident probability per movement is ranging about 0.23x10^{-4}, i.e 0.46x10^{-4} per flight considering that the two corresponding to a flight movements are carried out on the French territory. On the other hand, accidents analysis according to flight phases shows that the half of accidents occur "in course" or at the time of alteration of course in the air, which leads to a coefficient $p$ equal to 0.50. In the case of general aviation, "in course" phase may happen above an airport, which constrains to take into account the whole territory, without airport areas discrimination. Accidents that may affect a building can be classified as follows:

- Tail spin, lost of control and crash, 30%  
- Collision between aeroships, 5%  
- Breaking down of aircraft body during the flight, 2.5%  
- Fire or explosion, 1.0%  
- Miscellaneous, 10.0%

And aircraft strike angle with regard to the horizontal is considered as equal to 90° for tail spin, lost of control collision between aeroships, breaking down of aircraft body during the flight, fire or explosion and 45° for the other accident possibilities.

### 3.2 Air traffic

Available statistics concerning movements distribution in France versus weight of the aircraft (plate 4) allow to split up in two parts the probability study of a plane crash on a nuclear power plant with a division at 5.7 tons: aircrafts of a weight less than 5.7 tons fall into the general aviation category.

Movements number of commercial aircrafts on all French airports is known (460 000 movements in 1970); for general aviation evaluation of this number of movements gives about 6 300 000 in 1970.
AIR TRAFFIC DISTRIBUTION FOR 1970 IN FRANCE VERSUS PLANE WEIGHT
(3 442 810 movements for 1970)
3.2.1 - Commercial aviation

Air traffic above the French territory includes: domestic flights, international flights, using a French airport at the time of taking off or landing, international flights, traversing France, without stop. For 1970, statistics give the corresponding number of movements for domestic and international flights having used a French airport, which, assuming flights coming from or to abroad, gives 230 000 flights above France. Number of international flights over France, without stop, is estimated at first approximation to the same value, i.e 230000 flights. In the case of an occurring in France impact, the presented by this flight category risk is evaluated, in a pessimistic way, to the half of the presented by domestic flights risk (such an assumption takes into account the length of the flight above the territory, with regard to the entire flight length). Consequently, air traffic above France, in 1970 is equivalent to 350000 flights. Estimated yearly increase of air traffic for the next ten years is ranging about 7 to 8%. If we assume that this rate will remain constant up to the year 2000, air traffic above France evaluation for 1980, 1990 and 2000 is indicated in the following table (domestic equivalent number of flights per year):

<table>
<thead>
<tr>
<th>Year</th>
<th>7%</th>
<th>8%</th>
</tr>
</thead>
<tbody>
<tr>
<td>1970</td>
<td>350000</td>
<td>350000</td>
</tr>
<tr>
<td>1980</td>
<td>680000</td>
<td>740000</td>
</tr>
<tr>
<td>1990</td>
<td>1330000</td>
<td>1600000</td>
</tr>
<tr>
<td>2000</td>
<td>2600000</td>
<td>3500000</td>
</tr>
</tbody>
</table>

3.2.2 - General aviation

If we assume that all general aviation flights are domestic (this is checked for the majority of them and only 5% of the flights come from or to abroad), a flight may be assimilated to two movements on a French airport.

The number of movements in France, in 1970, for supervised or not by Secrétariat Général à l'Aviation Civile (S.G.A.C.) airports was equal to 6 300 000. Forecasted for the next ten years growth of the general air traffic being 5%, the number of flights above the territory, as far as this aviation category is concerned, may be evaluated to 5 000 000 flights for the year 1980.

Extrapolation beyond 1980 was not achieved owing to the lack of precise figures upon the future general aviation air traffic development.

3.3 - Building virtual area

3.3.1 - Virtual area definition

If we consider a building and an aircraft which impact trajectory makes an angle \( \alpha \) with the horizontal, virtual surface of a building is defined for this aircraft as the area of the shadow projected by the building on the ground, which corresponds to the area obtained by cylindrical projection, following the angle \( \alpha \), of the building apparent surface.

3.3.2 - General expression of the virtual surface

Let \( P(\alpha, \beta) \) be the building virtual surface for an impact angle \( \alpha \) with regard to the horizontal and a direction making an angle \( \beta \) with a reference vertical flat surface. The virtual surface \( P(\alpha, \beta) \) generally varies in function of...
the two parameters $a$ and $B$; but for each aircraft arriving on the building this virtual surface is well known, which leads to the definition of the building average probable virtual surface.

Let $A$ be the probability for an aircraft arriving on the building, with an impact angle $a$, and let $B$ be the probability that the impact direction of the aircraft makes an angle $B$ with the reference flat surface ($0 \leq a \leq \pi/2$ and $0 \leq B \leq 2\pi$).

Building average probable virtual surface, for any aircraft may then be defined with the following expression:

$$S_v = \int_0^{2\pi} \int_0^{\pi/2} P(a, B) \times A \times B \, da \, dB$$

In the general cases (i.e. parallelepipedic building, hilly geographical site, any aircraft impact angle), the average probable virtual surface $S_v$ is critical to evaluate. In certain particular cases, several simplifications are possible:

- probability $B (B)$ that aircraft impact direction makes an angle $B$ with the reference is generally uniform;

- if there is a revolution building (cylindrical or of PWR type building) the area projected on the ground $P (a, B)$ is a constant with respect to $B$;

- statistical analysis being made, it is visible that probability $A (a)$ of aircraft crashes able to interest the power plant is mainly distributed between impact angles equal to $45^\circ$ and $90^\circ$.

In order to simplify, remaining pessimistic in spite of all we admit in an approaching way that the half of aircrafts crashes vertically, when the other half is supposed to make an impact angle $a$ equal to $45^\circ$;

- in the case of a parallelepipedic building, function $P (a, B)$ varies in function of impact direction $B$, but it is possible to over-estimate the virtual surface, assuming that building is cylindrical, which diameter is equal to the diagonal line of the building roof.

For a cylindrical building which diameter is $D$ and height $H$, building virtual area is equal to:

$$S_v = \frac{\pi D^2}{4} + \frac{DH}{2}$$

With a PWR type building, diameter $D$ and height $H$, with an upper hemispherical roof, building virtual area is:

$$S_v = \frac{\pi D^2}{4} \left( \frac{3 + \sqrt{2}}{4} \right) + \frac{D}{2} \left( H - \frac{D}{2} \right)$$

3.3.3 Virtual surface determination in function of the aircraft

In the above computations, virtual surface determination was achieved assuming that the aircraft was a physical point. Aircraft dimensions being not negligible with regard to an industrial building dimensions, virtual surface is determined taking into account reference aircraft for both commercial and general a-viations.

For the most unfavourable case, reference aircraft that we selected for virtual surface determination is the BOEING 747 for commercial aviation, (dimension to take into account corresponds to the distance between body and the extremest engine axes, i.e 21 m for such an aircraft) and an aircraft of which the span is 11 m for general aviation (distance to take into account 5.5m).

For taking into account the plane dimensions, we artificially increased buil-
Cylindrical building \( S_v = \pi \left( \frac{D + d}{4} \right)^2 + \frac{D + d}{2} H \)

PWR type building \( S_v = \pi \left( \frac{D + d}{4} \right)^2 \left( 3 + \sqrt{2} \right) + \frac{D + d}{2} \left( H - \frac{D}{2} \right) \)

Calculation of a PWR type building virtual area which diameter is \( D = 66 \text{m} \) and height \( H = 80 \text{m} \) (case of Super-Phénix building reactor) in function of each reference aircraft gives the values as follows:

- Commercial aviation (\( d = 21 \text{m} \))
  \[ S_v = 8600 \text{ m}^2 \]
- General aviation (\( d = 5.5 \text{m} \))
  \[ S_v = 6100 \text{ m}^2 \]

4 - EVALUATION OF THE AIRCRAFT CRASH ON A NUCLEAR POWER PLANT PROBABILITY:

Knowing each parameter value for the expression giving crash probability of a plane on a considered power plant, the following table was established taking into account the two commercial aviation growth assumptions (7 and 8\%), crash probability per flight being for commercial aviation : \( 0.30 \times 10^{-5} \), and for general aviation : \( 0.40 \times 10^{-4} \), with respectively 15\% and 50\% of the commercial and general aviation accidents able to concern a nuclear power plant:

<table>
<thead>
<tr>
<th>Growth rate</th>
<th>Commercial aviation</th>
<th>General aviation</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>1980</td>
<td>1990</td>
</tr>
<tr>
<td>PWR</td>
<td>7 %</td>
<td>0.44 \times 10^{-8}</td>
</tr>
<tr>
<td>Type building</td>
<td>8 %</td>
<td>0.45 \times 10^{-8}</td>
</tr>
</tbody>
</table>

the values given in the table are yearly probabilities.

It can be noted that : for commercial aviation, crash probability of a plane on nuclear power plant is, in all the cases, less than \( 2 \times 10^{-8} \) per year for the year 2000, and for general aviation, probability is clearly larger, we find a value ranging about \( 1 \times 10^{-6} \) per year, already in 1980.

5 - CONCLUSION

The values given in the table above set up a computed gross result which includes a certain number of assumptions on the stepping in computation parameters ; it seems to us essential to add a certain number of considerations of a qualitative nature to this numerical result, taking into account of which would rather reduce the announced probabilities:

- envisaged buildings are considered separately, when in fact the power plant reactor building is, in most cases, surrounded with outlying structures (machinery hall, auxilliary rooms, etc...) that serve as protection to the building and involve an impact virtual surface reduction, that is sometimes significant.

- in the case of commercial aviation, the aircraft took as reference (BOEING 747) increases, also and in an important manner, building virtual sur-
face. In fact, obviously, the flying park will not be formed with only "heavy carriers" and the "average" virtual surface to take into account should be balanced by respective proportions of different aircraft types, in the year 2000. - we believe to be pessimistic in considering that the air transportation safety would be equivalent, in the year 2000, to the 1970 one.

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On the Use of Leak Path Analysis in Fault Tree Construction for Fast Reactor Safety

R. R. Fullwood and R. C. Erdmann
SAI Services, McLean, Va.

ABSTRACT

This paper describes a method for determining the top of a fault tree for an accidental release of radioactivity, with application to the FFTF. The analysis leads from design drawings and related information to a logical expression for the failure paths. This algebraic expression, when reduced, is represented by a fault tree. Additional trees are also drawn for other parts of the system. The results of the method are compared to alternate schemes and the concept of fault tree completeness is considered.

I. INTRODUCTION

Nuclear safety is primarily concerned with protecting the environment from radioactivity through the use of multiple barriers. Leak path analysis is an algebraic method that enumerates the barriers encountered along all paths that can lead from the radioactive source to the environment. An expression for a leak path is composed of the intersection of the failure of barriers along a path and its overall effect.

By examining the design of a reactor it is possible to define successive barriers through which radioactive sources must pass to be released in an uncontrolled manner. Often it is possible to redraw the reactor from a barrier viewpoint. This "labyrinth" drawing then leads rather easily to an algebraic expression for a path from the source to the environment.

There corresponds to this set of logic expressions, or algebraically reduced versions of them, a fault tree which, when drawn, exhibits all possible means of accidental releases. Subsystem behavior can be examined through fault trees (e.g., scram system fault analysis) which would then be combined beneath the top of the tree obtained by a leak path analysis, to provide system reliability data.

An alternative to the leak path method is the event tree approach based upon decision theory, used in the Reactor Safety Study(1). In this latter method one begins with an initiating incident (e.g., a large pipe break) and traces through system behavior with time as each subsystem is activated. Given, an event tree, one can rather easily draw a fault tree corresponding to it.

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However, the fault tree so drawn is based upon that one initiating event. To generate a complete top of the tree requires one to postulate a comprehensive set of initiating events.

It is well known that fault trees are not unique for a given system even when they reduce to the same algebraic form. The only thing that is unique is the equation itself. The method described here strives for uniqueness by factoring the equation into the most compact form. The path sets so obtained may correspond to the minimum path sets useful in numerical evaluation although this latter conjecture has not been proved as yet.

Paralleling any fault tree analysis of system reliability is a consequence analysis to describe the radioactive source terms of importance. These two components of an accident can then be incorporated into a risk analysis for a given reactor(2). Consequence analysis is based on the physical behavior of the system and is often aided by knowledge of its relevance from a likelihood viewpoint.

In Section II of this paper, we provide a simple example of leak path analysis followed by Section III containing a leak path analysis of the FFTF. In Section IV is drawn the fault tree corresponding to the leak path expression developed in Section III. Additional system fault trees are presented in Section V. Section VI compares this work briefly with past analysis and Section VII contains a summary of the paper.

II. A SIMPLIFIED EXAMPLE

To provide an introduction to the methodology of leak path analysis we have chosen to examine an internal combustion engine for the top event of "Low Cylinder Compression". A sketch of the cylinder from such an engine is given in Figure 1.

The compression can be low due to Head Gasket leakage (HG), Exhaust Valve leakage (EV), Intake Valve leakage (IV), or Spark Plug leakage (SP). There could be a hole in the piston (P) but to leak past the piston rings requires leakage past Piston Ring 1 AND Piston Ring 2 (PR1·PR2), where multiplication is used to indicate intersection or AND. This may be concisely stated as an equation

\[ LCC = \text{low cylinder compression} \]
\[ LCC = \text{HG} + \text{EV} + \text{IV} + \text{SP} + \text{P} + (\text{PR1} \cdot \text{PR2}) \]

where summation is used for union or OR.

Thus by examining all possible paths for loss of compression one is led to a "complete" listing of the possible means of failure. Moreover, the analysis has led from a design sketch directly to an algebraic equation. Usually one would draw a fault tree from the design sketch and then transform this fault tree into an algebraic statement. In this case we have an equation and can transform it to a fault tree. This is done by associating the usual symbols for the OR and AND operations. Figure 2 is a sample fault tree constructed from this equation. In this simple case, there is a unique correspondence between the equation and the tree. In a more complex case such as FFTF safety systems, the shape of the tree depends on how the equation is factored.
FIGURE 1 Sketch of Internal Combustion Engine

FIGURE 2 Fault Tree for Low Cylinder Compression
III. LEAK PATH ANALYSIS OF THE FFTF

For this analysis, it will be assumed that the FFTF is under normal power operation before the accident and the accidental release of radioactivity into the ground will be ignored, while we concentrate on the airborne release.

To begin the analysis, the sources of radiation must be identified; they are the fuel, fission and activation products in the fuel rods (F). In addition, in the FFTF the primary sodium loop will be activated (S2) and thus constitutes a source. For the products in the fuel to escape, they must diffuse out of the fuel matrix (M), pass through the clad (C) and enter into the coolant (NA). This source is: \( S_1 = F \cdot M \cdot C \cdot NA \).

It may seem pedantic to explicitly state that the products can be held up in the matrix, contained by the cladding and retained in the coolant but these are important considerations when calculating the consequences of an accident. The sodium coolant is especially effective at retaining fission products if the temperature is kept low.

Utilizing this initial source, one now requires a diagram which clearly depicts the pathways of accidental release. By examining design drawings in refs. 3 to 8, we have drawn Figure 3 from which our leak path analysis can proceed.

The first leak path considered will be from the primary coolant, through the pressure vessel (PV) through the reactor cell (RC), through the containment (CB) and through the equipment building (EB) to the atmosphere:

\[
S \cdot PV \cdot RC \cdot CB \cdot EB
\]

where

\[
S = S_1 + S_2
\]

The second path follows the same route but instead of leaking through the containment building, it passes through the wiring penetration (WP).

\[
S \cdot PV \cdot RC \cdot WP \cdot EB
\]

This procedure is repeated for each of the penetrations of the containment building. Since these expressions are combined as union (added), it is convenient to define

\[
CP = CB + WP + PPN + VO + VI + ETL + EA + PA.
\]

Hence we have

\[
S \cdot PV \cdot RC \cdot CP \cdot EB
\]

as one set of leak paths.

A failure of the primary piping in the reactor cell (PR) leads to a similar expression but the loop isolation valves must also fail.

Hence

\[
S \cdot PR \cdot RV \cdot RC \cdot CP \cdot EB
\]
Figure 3: Labyrinth for FFTF
combining eqs. (5) and (6) yields

\[ S \cdot (PV+PR\cdot RV) \cdot RC \cdot CP \cdot EB. \]  \hspace{1cm} (7)

Were the guard vessel to fail after a failure of the pressure vessel or the primary piping and the isolation valves, then sodium concrete reactions might occur volatilizing some radioactive species. Thus we have

\[ S \cdot (PV+PR\cdot RV) \cdot GVR \cdot RC \cdot CP \cdot EB. \]  \hspace{1cm} (8)

In this case no account is taken of sodium overflowing the reactor guard vessel without its failure because it is designed to take the full sodium inventory of the FFTF.

An interval event may take place in the reactor core causing the core to melt down. It would then have to penetrate the reactor vessel, and the guard vessel, it would also have to penetrate the core catcher to reach the concrete. Thus

\[ S \cdot PV \cdot GVR \cdot CC \cdot RC \cdot CP \cdot EB. \]  \hspace{1cm} (9)

In one of the three primary heat exchanger cells, a failure of the shell (PS) or a failure of the primary piping in the primary cell (PP) can result in the path

\[ S(PS+PP) \cdot RV \cdot PC \cdot CP \cdot EB. \]  \hspace{1cm} (10)

Moreover, the failure of these components can result in a release through failure of the primary guard vessel (GVP) resulting in a sodium concrete interactions:

\[ S(PS+PP) \cdot RV \cdot GVP \cdot PC \cdot CP \cdot EB \]  \hspace{1cm} (11)

It is also possible for the sodium to overflow the primary guard vessel GVO although this cannot be done under pony motor operation. Thus

\[ S(PS+PP) \cdot RV \cdot GVO \cdot PC \cdot CP \cdot EB. \]  \hspace{1cm} (12)

If there is failure of the primary-secondary interface (PSH) in the primary heat exchanger, although the pressure is normally higher on the secondary side, it may be possible by either diffusion or pressure reversal to get radioactivity in the secondary loop. This can lead to a release to the atmosphere by failure of the secondary piping (SP) or the secondary heat exchanger shell (SS):

\[ S \cdot PSH \cdot RV \cdot (SS+SP) \cdot EB. \]  \hspace{1cm} (13)

If the secondary tube failed releasing sodium to the cooling air, it would yield outside the equipment building:

\[ S \cdot PSH \cdot RV \cdot ST \]  \hspace{1cm} (14)
These then constitute the possible paths for accidental radioactive release from the FFTF design, as we know it.

Collecting and summing statements 7-14 and rearranging gives the atmospheric release (AR):

\[
AR = S \cdot \left( (PV + PR \cdot PV) \cdot (GVR + 1) + PV \cdot GVR \cdot CC \right) \cdot RC \\
+ \left( (PS + PP) \cdot RV \cdot (1 + GVP + GVO) \cdot PC \right) \\
\cdot \left( (CB + WP + PPN + VO + VI + ETL + EA + PA) \cdot EB \right) \\
+ PSH \cdot RV \cdot (SS + SP) \cdot EB + PSH \cdot RV \cdot ST \right)
\]

(15)

For convenience the meaning of the symbols is given in Table I. The mincut sets are found by expanding equation 15 into intersection sets. Some of these are presented in Table II.

**Table I: Meaning of Symbols in Equation 15**

<table>
<thead>
<tr>
<th>Symbol</th>
<th>Meaning</th>
</tr>
</thead>
<tbody>
<tr>
<td>C</td>
<td>Cladding</td>
</tr>
<tr>
<td>CC</td>
<td>Core Catcher</td>
</tr>
<tr>
<td>CB</td>
<td>Containment Building</td>
</tr>
<tr>
<td>EA</td>
<td>Emergency Airlock</td>
</tr>
<tr>
<td>EB</td>
<td>Equipment Building</td>
</tr>
<tr>
<td>ETL</td>
<td>Equipment Transfer Lock</td>
</tr>
<tr>
<td>F</td>
<td>Fission Products, Fuel, and Activation Products</td>
</tr>
<tr>
<td>GVP</td>
<td>Guard Vessel-Primary Heat Exchanger Cell</td>
</tr>
<tr>
<td>GVO</td>
<td>Guard Vessel Overflow in Primary Cell</td>
</tr>
<tr>
<td>GVR</td>
<td>Guard Vessel-Reactor</td>
</tr>
<tr>
<td>M</td>
<td>Fuel Matrix</td>
</tr>
<tr>
<td>PC</td>
<td>Primary Cell Boundary</td>
</tr>
<tr>
<td>PP</td>
<td>Primary Piping in Primary Heat Exchanger Cell</td>
</tr>
<tr>
<td>PPN</td>
<td>Piping Penetrations of Containment</td>
</tr>
<tr>
<td>PR</td>
<td>Primary Piping in Reactor Cell</td>
</tr>
<tr>
<td>PS</td>
<td>Primary Heat Exchanger Shell</td>
</tr>
<tr>
<td>PSH</td>
<td>Primary-Secondary Heat Exchanger Boundary</td>
</tr>
<tr>
<td>PV</td>
<td>Pressure Vessel</td>
</tr>
<tr>
<td>RC</td>
<td>Reactor Cell Boundary</td>
</tr>
<tr>
<td>RV</td>
<td>Reactor-Primary Loop Isolation Valves</td>
</tr>
<tr>
<td>SP</td>
<td>Secondary Piping Outside of Containment</td>
</tr>
<tr>
<td>SS</td>
<td>Secondary Heat Exchanger Shell</td>
</tr>
<tr>
<td>ST</td>
<td>Secondary-Testing Tubes</td>
</tr>
<tr>
<td>S2</td>
<td>Activated Sodium Radioactivity Source</td>
</tr>
<tr>
<td>VI</td>
<td>Ventilation In</td>
</tr>
<tr>
<td>VO</td>
<td>Ventilation Out</td>
</tr>
<tr>
<td>WP</td>
<td>Wiring Penetration</td>
</tr>
</tbody>
</table>

**Table II: MinCut Sets**

<table>
<thead>
<tr>
<th>Set</th>
<th>Symbols</th>
</tr>
</thead>
<tbody>
<tr>
<td>4</td>
<td>S \cdot PSH \cdot RV \cdot ST</td>
</tr>
<tr>
<td>5</td>
<td>S \cdot PSH \cdot RV \cdot SS \cdot EB</td>
</tr>
<tr>
<td>6</td>
<td>S \cdot PV \cdot GVR \cdot RC \cdot CB \cdot EB</td>
</tr>
<tr>
<td>7</td>
<td>S \cdot PS \cdot RV \cdot PC \cdot GVP \cdot CB \cdot EB</td>
</tr>
<tr>
<td></td>
<td>\cdots (47 more)</td>
</tr>
<tr>
<td></td>
<td>\cdots (5 more)</td>
</tr>
<tr>
<td></td>
<td>\cdots (31 more)</td>
</tr>
</tbody>
</table>
IV. FAULT TREE CONSTRUCTION

Figure 4 and the linking fault tree Figure 5 is a fault tree representation of equation 15. The design of a fault tree from a Boolean equation is not unique -- it depends on how the equation is factored. In preparing Figures 4 and 5, an effort was made to use a compact algebraic form (terms appear only once) leading to a compact fault tree. For the fault tree to progress from cause to effect equation 15 was constructed from the outside to the inside of the brackets.

A slight change occurred in the construction of Figure 4 by grading the severity of the releases from the reactor vessel. Of least severity was a vessel crack leaking coolant that is retained by the guard vessel. The next highest severity results in sodium-concrete reaction due to a large break that also results in failure of the reactor guard vessel. Finally, there is the catastrophic core melt-down accident that fails a core-catcher, introduced as an option to investigate the advantages of this device.

V. SYSTEM FAULT TREES

To complete the FFTF fault tree for which Figure 4 provides the skeleton, requires the construction of system fault trees for each of the entry points. These system fault trees can be constructed using leak path analysis or by conventional fault tree methods. To illustrate, the event reactor melt-down will be extended through "Pressure Vessel Melt Through (PVM)" and "Failure of the Core Catcher (CC)".

In constructing PVM, reference was made to the EBR-1 melt-down which resulted from a slow scram and the Fermi melt-down which resulted from flow blockage. For this latter event, it is assumed that noise analysis will be used on the FFTF to detect the blockage and corrective action could be taken. These events are depicted in Figure 6.

The natural events included are tornado destruction of the tertiary loop so it is not possible to cool the reactor. This is a low probability event because of the location of FFTF. Earthquake disruption of the core is unlikely but is shown in Figure 6 and has been considered in the design. A rapid reactivity insertion can possibly lead to severe pressure generation. Control rod ejection can lead to a local high power density resulting in sodium boiling, vapor blocking and local melting which could propagate to more of the core. The last major initiating event considered is a loss of on-site power which, in spite of corrective cooling, could cause over-heating because the secondary and tertiary loops might not work. If this is coupled with failure to scram, the problems are compounded.

Figure 7 is a fault tree for the failure of an ex-vessel core catcher. Although reference is made to Catton and Erdmann, the concepts are not the proposed designs. The fault tree indicates the core catcher might fail from loss of heat transfer (HT), capability which could also have been the initiating event.
FIGURE 4: TOP OF FAULT TREE
FIGURE 5: UPC LINKING TREE
FIGURE 6: CORE MELT ACCIDENT
Figure 7: Core Retainer Failure
thus not providing complete protection. A further shortcoming of these designs would result from core damage of the guard vessel possibly allowing sodium concrete reactions in addition to the sodium fuel reactions already occurring. The possible design error "Taylor Instability" was included because of the severe density variations.

VI. COMPARISON WITH PREVIOUS ANALYSIS

McLaughlin\textsuperscript{12} has performed a fault tree analysis on the FFTF. A related analysis but aimed at a 1,000 MWE LMFBR has been performed by Godbout\textsuperscript{13}. There is also related work in Graham\textsuperscript{7} and in an LMEC-Memo\textsuperscript{14}.

Figure 8 is a portion of the top of the fault tree taken from McLaughlin\textsuperscript{12}. He takes as the top event, "URR Outside of Containment" while we include the equipment building because it will tend to remove condensibles and particulates. It will not, however, prevent the total escape of all radioactivity and this fact must be kept in mind.

McLaughlin\textsuperscript{12} does not consider releases from the primary heat exchanger cells, releases from the tertiary cells or the extra protection provided by the guard vessels or the loop isolation valves that in this analysis are involved in every failure except the pressure vessel itself.

The general structure of his tree otherwise resembles Figure 4 rather well and many of his fault trees could be used as sub-trees in Figure 4. Certainly any extension of this work would draw on McLaughlin\textsuperscript{10} and Godbout\textsuperscript{11}.

VII. SUMMARY

Leak path analysis, which is particularly useful for analysing barrier systems, follows logically from its close correspondence to the physical system. Its algebraic character allows convenient manipulation and reduction before committing to a fault tree representation. In the FFTF example presented here, a labyrinth was used to assist in writing the Boolean equations. This was done primarily for clarification and is not necessary.

In performing a fault tree analysis of a nuclear fuel reprocessing plant, no labyrinth was constructed because of plant complexities. Equations were written directly working from the source documents. These path equations were then combined, factored into most compact form, and transformed into a fault tree drawing.

The leak path method achieves completeness when all possible ways to penetrate a barrier are enumerated. Although completeness cannot be guaranteed, it is assisted by the systematic enumeration and the labyrinth drawing. We have found the leak path approach to be quite applicable to the FFTF and our results agree with those of reference 12.

The concept of successive barriers for the containment of radioactivity, used so successfully in nuclear reactors, can aid in reliability quantification of fast reactors through this leak path approach. Adequate design information is, of course, essential in this approach, and completeness in the fault tree appears possible through its use.
FIGURE 8: TOP OF FFTF FAULT TREE
FROM MCLAUGHLIN
REFERENCES


"AN ESTIMATE OF LMFBR STEAM GENERATOR SYSTEM RELIABILITY AND AVAILABILITY"

N.W. Brown, R.K. Stitt, R.J. Wilson

ABSTRACT

A probabilistic evaluation of a reference steam generator system is discussed. The study was conducted in support of the AEC LMFBR Steam Generator Systems Development Program. The objective of the study was to help in identifying the relative importance of critical development areas. The study estimates the probability of structural damage to the secondary sodium system boundary and the availability of a single loop of a three loop reference plant. Critical development needs and their relative importance to reliability and availability goals are identified based on the probabilistic evaluations.

Introduction

A reliability study was conducted as a part of the AEC LMFBR Steam Generator Systems Development Program initiated in 1972. The objective of the study was to evaluate the reliability of a representative steam generator system to help in identifying the critical development needs. A probabilistic approach to the problem of identification of development priorities is particularly good because the development priority is based on the effect the information has on the probability of the system functioning. The approach used was to identify reliability and availability goals for the steam generator system and to conduct probabilistic, reliability and availability evaluations of the reference system to determine those components, subsystems, or phenomena, which have the largest impact of the identified goals. One loop of the steam generator system analyzed is shown schematically in Figure 1.

Summary and Conclusions

A probabilistic evaluation of a reference steam generator system was performed to identify the components which have a major impact on achieving reliability and availability goals. The safety goal utilized for comparative purpose is that the probability of significant structural damage to the secondary sodium coolant boundary, as a result of sodium-water reactions, is less than $10^{-7}$ per year. The steam generator availability goal used is that the availability of a single loop is 98%, which is based on an unavailability of 0.12% for the total three loop steam generator system.

A fault tree of possible sequences of events which could result in damage to the secondary sodium boundary as the result of a sodium-water reaction was constructed. The probability of each component in the fault tree was determined from existing failure data and consultation with engineers working on evaporator and superheater designs. The PREP-KITT computer codes were used to perform numerical calculations to evaluate the possibility of achieving the safety goal. A base case was established which used the best estimate values for the input probabilities. Numerical results for this case gave a probability...
for damage to the secondary coolant boundary of $11.7 \times 10^{-5}$ per plant year. Parametric studies were then performed to determine which components in the fault tree were important in establishing the calculated value, and thus would be an important area for further study and development to reduce the assigned values and achieve the safety goal of $10^{-5}$ per plant year.

The availability analyses indicated that sodium-water leaks are also the major contributor to unavailability of the steam generator, and thus work to reduce the occurrence of leaks which require shutdown will directly improve the availability. Since the down time required to find and repair these leaks is a major factor in the unavailability, design features which help to reduce the down time will also be important. The availability of a single steam generator loop was calculated to be 90.6% if the mean time to repair a steam generator tube leak was 1000 hours and 97% if this repair time is only 200 hours. With a mean repair time of 200 hours, the evaporators and superheaters account for approximately 50% of the system unavailability. This indicates that steam generator leaks and their repair are the most important area for possibilities of improving system availability to the goal 98%. Other areas important to system availability are Na Pressure seals, rupture discs, recirculation pumps, and vapor traps. These latter areas contribute 3.0 to 10.0% individually of the total unavailability. Although these latter areas are not clearly as important as the heat transfer components to plant availability, the chance for improvement in these areas may be high and should not be neglected when considering system availability.

Fault Tree Analysis of the Safety Objective

The steam generator system presents a potential hazard because of the possibility of releasing large amounts of secondary sodium or causing THX tube damage as a result of a sodium-water reaction. The safety goal was stated in terms of the probability of structural damage to the secondary sodium coolant boundary as the result of a sodium-water reaction. This goal excludes considering damage to this boundary from causes other than sodium-water reaction. This was done to simplify the problem and also because it was considered that sodium leaks not accompanied by water reaction had not the public concern of sodium-water leaks. The latter has a potential for involving the containment barrier of the reactor or the reactor itself, if the system design is not properly done. The goal neglects the effects of loop interaction; for example, reactions in one steam generator affecting other steam generator loops. It is assumed that criteria will be produced elsewhere to cover these neglected items and that the requirements of safety in these areas will not require the development attention to satisfy them.

A value of $10^{-5}$ per plant year was chosen as the goal value for the probability of a sodium-water reaction resulting in structural damage to the secondary coolant boundary. This value is based on probabilistic goals stated for severe accidents associated with nuclear facilities. To evaluate the possibility of achieving this goal, the fault tree of possible sequences of events, which could result in damage to the secondary sodium boundary was constructed and analyzed using the PREP-KITT computer codes. Figures 2 and 3 present this fault tree. The term TOP is given to the most undesired event of the fault tree, i.e., structural damage to the secondary sodium coolant boundary. A list of the other components in the fault tree is given in Table I.

The spectrum of leaks was divided into six classes of leaks, but was not separated as to cause of the leak. The relative rate of occurrence of these six classes of leaks was set arbitrarily with large leaks much less probable than small leaks. The classes of leaks were not defined quantitatively, but were qualitatively defined as follows:
Figure 1. Steam Generator System

Legend:
- Dashed line: WATER
- Solid line: STEAM
- Black line: SODIUM
- Gray line: SODIUM DRAIN
- Gray line with dots: REACTION PROD.
- Crossed lines with arrows: VALVE
- Crossed square: RUPTURE DISK
- Crossed triangle: VENT TO ATMOSPHERE

One Loop of Three
Figure 2 (Part 1). Fault Tree of $\text{H}_2\text{O}/\text{Na}$ Leaks in the Steam Generator Which Result in a Single Large Leak

1. SINGLE LARGE LEAK (SSL)
2. SML OCCURS SUDDENLY
3. SINGLE MEDIUM LEAK (SML)
4. MANY SMALL LEAKS AND A SINGLE MEDIUM LEAK (MSL & SML)
5. SSL PROPAGATES TO SLL
6. MSL & SML D-ITL PROPAGATES TO SLL
7. SML D-ITL PROPAGATES TO SLL
8. MSL D-ITL PROPAGATES TO MSL & SML
9. MANY SMALL LEAKS (MSL)
10. SML D-ITL PROPAGATES TO MSL & SML
11. SSL ND/I PROPAGATES TO MSL
12. SINGLE SMALL LEAK (SSL) OCCURS SUDDENLY
13. SML OCCURS SUDDENLY
14. MSL OCCURS SUDDENLY
15. SSL ND/I PROPAGATES TO SLL
16. SINGLE MEDIUM LEAK (SML)
17. MSL & SML D-ITL PROPAGATES TO SLL
18. MSL D-ITL PROPAGATES TO SLL
19. SML D-ITL PROPAGATES TO SLL
STRUCTURAL DAMAGE TO SECONDARY SODIUM BOUNDARY

Figure 3 (Part 2). Fault Tree of H₂O/Na Leaks in the Steam Generator Which Result in Structural Damage to Secondary Sodium Boundary
(1) Single Small Leak (SSL) is a leak which is at the detection threshold of the leak detection system. The probability of detecting this leak is of the order of 50%.

(2) Single Medium Leak (SML) is a leak which has a high probability of detection, but is not large enough to pressurize the system, and thus will not activate the pressure relief system.

(3) Single Large Leak (SLL) is the minimum leak for which the pressure relief system is designed to function. It was assumed that this is a leak equivalent to the guillotine rupture of a single evaporator tube.

(4) Many Small Leaks (MSL) are two or more small leaks which have a total leak rate equivalent to a single medium leak. Many small leaks do not pressurize the system.

(5) Many Medium Leaks (MML) are two or more medium leaks which have a combined leak rate equivalent to a single large leak. Many medium leaks will pressurize the system and activate the pressure relief system.

(6) Many Large Leaks (MLL) are two or more large leaks and have a combined leak rate equivalent to the rupture of two or more tubes in the steam generator. This class includes all leaks from this level up to and including the maximum leak that can occur.

**TABLE I**

**COMPONENTS OF FAULT TREE FOR THE STEAM GENERATOR SYSTEM**

<table>
<thead>
<tr>
<th>COMPONENT NUMBER</th>
<th>FAILURE INTENSITIES</th>
<th>PROBABILITIES</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>Failure Intensity for Single Small Leaks (SSL)</td>
<td>SSL Not Detected or Ignored and Becomes MSL</td>
</tr>
<tr>
<td>2</td>
<td>Failure Intensity for Single Medium Leaks (SML)</td>
<td>MSL Detected but Ignored too Long Becomes MSL and SML</td>
</tr>
<tr>
<td>3</td>
<td>Failure Intensity for Single Large Leaks (SLL)</td>
<td>SML Detected but Ignored too Long Becomes MSL and SML</td>
</tr>
<tr>
<td>4</td>
<td>Failure Intensity for Many Small Leaks (MSL)</td>
<td>SSL Not Detected or Ignored and Becomes SML</td>
</tr>
<tr>
<td>5</td>
<td>Failure Intensity for Many Medium Leaks (MML)</td>
<td>SLL Pressure Relief Inadequate or Malfunctions Becomes MLL</td>
</tr>
<tr>
<td>6</td>
<td>Failure Intensity for Many Large Leaks (MLL)</td>
<td>SSL Not Detected or Ignored too Long Becomes MLL</td>
</tr>
<tr>
<td>7</td>
<td>SSL Not Detected or Ignored and Becomes MSL</td>
<td>MSL Detected but Ignored too Long Becomes MSL and SML</td>
</tr>
<tr>
<td>8</td>
<td>MSL Detected but Ignored too Long Becomes MSL and SML</td>
<td>SML Detected but Ignored too Long Becomes MSL and SML</td>
</tr>
<tr>
<td>9</td>
<td>SML Detected but Ignored too Long Becomes MSL and SML</td>
<td>SSL Not Detected or Ignored and Becomes SML</td>
</tr>
<tr>
<td>10</td>
<td>SSL Not Detected or Ignored and Becomes SML</td>
<td>SLL Pressure Relief Inadequate or Malfunctions Becomes MLL</td>
</tr>
<tr>
<td>11</td>
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<td>SSL Not Detected or Ignored too Long Becomes MLL</td>
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<td>MSL Detected but Ignored too Long Becomes MLL</td>
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<td>MSL Detected but Ignored too Long Becomes MLL</td>
<td>MSL Detected but Ignored too Long Becomes MLL</td>
</tr>
<tr>
<td>14</td>
<td>MSL Detected but Ignored too Long Becomes MLL</td>
<td>SSL Not Detected or Ignored and Becomes SLL</td>
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TABLE II
RESULTS OF FAULT TREE ANALYSIS OF STEAM GENERATOR SYSTEM

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** Top (10⁻³/Yr) 11.5 9.2 16.5 13.7 19.3 15.4 16.2 34.4 1.0 1.1 1.2

** Base Case  * Base Case Value Used
To establish a basis for the input probability values, information was solicited from senior technical personnel familiar with the steam generator system technology. The information obtained was not directly applicable to the fault tree constructed, but it provided a bases for a number of component failure probabilities. In particular, it provided the bases for the first estimate of total leak probability and the reliability of the pressure relief system. Case 1 in Table II presents the input probabilities based on the survey. Case 1 was used as the base case to perform sensitivity and parametric studies to determine which event probabilities were important in determining the value of TOP and the value, or range of values, for these probabilities that would be required to reduce TOP to $10^{-5}$ per plant year.

These sensitivity studies were divided into three groups: (1) a study of the effects of varying the leak probabilities, (2) a study of the effects of varying the probability of failure or inadequacy of the steam generator pressure relief system, and (3) a study to determine which leak propagation probabilities are important in determining the value of TOP.

A series of cases were run to determine the effects of varying the way in which the .75 leak/year was divided among the six leak classes. Case 2 was run, assuming that all leaks are small leaks initially. This is consistent with the beliefs of many of the people surveyed. Considering only small leaks reduced the value of TOP to $9.17 \times 10^{-5}$, this indicates that the major contributor to TOP in Case 1 is small leaks. Increases of a factor of 10 in the occurrence of any one of the leak classes larger than SSL, increases the value of TOP less than a factor of two (Cases 3 through 7). Increasing the rate of occurrence of all of the larger leaks by a factor of 10 increases TOP by about a factor of three (Case 8). Reducing all the initial leak probabilities by a factor of 10 would result in a value for TOP a factor 10 smaller than the case case or $1.17 \times 10^{-5}$ per plant year. This would mean experiencing only one leak in about 12 1/2 years of loop operation.

The next cases run were to determine the effects of changing the probability of failure of the pressure relief system. Since these are the last values in the fault tree, they have a very direct result on the value of TOP. Various combinations of the values for components 24, 25, and 26, were tried to find a set of values, which appear to be within reason, that could result in TOP = $10^{-5}$ per plant year. Such a set is shown in Case 9. This would require that the pressure relief system have a .9999 probability of success for the design leak rate, single large leaks (26) or many medium leaks (25), and a .8 probability of success for all leaks greater than the design leak, many large leaks (24). Since these probabilities include the uncertainty associated with calculating the pressure which results from a given leak as well as the ability of the system to adequately relieve these pressures, these high probabilities of success may not be reasonable.

The third parametric study varied each of the probabilities of propagation of leaks to determine which of these values were important in determining the value of TOP. For the first case (Case 10) all propagation probabilities were set to zero. The values of the rates of occurrence of leaks, and the probabilities of failure of the pressure relief system, were the base case values (Case 1). This resulted in a value of TOP = $1.1 \times 10^{-5}$ per year. This shows that reduction of the propagation probabilities alone will nearly achieve the goal of $10^{-5}$ per plant year. The components, which made a significant change in TOP when they were reduced a factor of 10, are shown in Table III, in the order of their effect. Additional information about these probabilities would be of greatest importance in determining the reliability of the steam generator system.
TABLE III
IMPORTANT LEAK PROPAGATION PROBABILITIES

<table>
<thead>
<tr>
<th>COMPONENT</th>
<th>BASE VALUE</th>
<th>NEW VALUE</th>
<th>TOP</th>
<th>% CHANGE TO TOP</th>
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<tbody>
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<td>.015</td>
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<td>.15</td>
<td>.015</td>
<td>$9.1 \times 10^{-5}$</td>
<td>21.8</td>
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<td>.01</td>
<td>.001</td>
<td>$9.1 \times 10^{-5}$</td>
<td>21.4</td>
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</table>

Even though the absolute value of the input probabilities and thus the calculated value for TOP have not been firmly established, the relative importance of various components in the fault tree in determining the probability of structural damage to the secondary coolant boundary should be valid since all were arrived at by the same logic arguments and engineering judgment. This information can be used to assign relative importance to future development programs which could be conducted to assure that the steam generator system will meet required reliability goals.

Availability Goals and Analysis

An availability goal (hours per year plant is capable of generating electric power divided by total hours per year, in percent) for the steam generator system was established which is based on achieving an overall plant availability of about 80%. An allocation of availability was made to the total plant components and it indicated that a contribution of 0.12% unavailability or less, is required of the steam generator system for the plant to achieve the availability goal desired.

The plant capacity factor (the total generation, in megawatt-hours, in a period divided by the product of the period hours times the capacity) is another factor that must be considered in plant performance. It has been assumed here that if the availability is near 80% for a three loop plant that is capable of operating on two or three loops only, then the plant capacity factor will be acceptable. If, in fact, the 80% availability factor is achieved by operating the plant at partial load on two loops for major fraction of the time, the importance of the availability number would be significantly reduced.

A Failure Mode and Effects Analysis (FMEA) was prepared to identify those components and failure modes which would result in shutdown of one loop of the steam generator system. This analysis was a prelude to numerical analysis performed to estimate the loop availability. Failure rates and mean repair times were established (for those failures which resulted in shutdown of the loops) by utilizing available failure data and/or engineering judgment.

A success diagram of all failure events, which would result in shutdown of one-loop of the steam generator system, was prepared from a failure mode and effects analysis as a model for computing system availability. The model includes: feedwater piping (excludes feedwater pump), steam drum, recirculation system, two evaporators, two superheaters, piping to turbine (excludes turbine), secondary sodium piping (excludes IHX, secondary sodium pump), instrumentation and protection system. Failure of any component in the model leads to shutdown of
<table>
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<th>( \lambda_{SH} )</th>
<th>( \lambda_{E} )</th>
<th>( \lambda_{SD} )</th>
<th>( \tau_{SH} - \tau_{E} )</th>
<th>( \tau_{SD} )</th>
<th>( \tau_{MIN} )</th>
<th>( A )</th>
<th>( \bar{A} )</th>
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<td>9.383</td>
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*WHERE \( \lambda_{SH} \) = Failure Rate of Superheater, Failures/10^6 Hours*

*\( \lambda_{E} \) = Failure Rate of Evaporator, Failures/10^6 Hours*

*\( \lambda_{SD} \) = Failure Rate of Steam Drum, Failures/10^6 Hours*

*\( \tau_{SH} \) = Repair Time of Superheater, Hours*

*\( \tau_{E} \) = Repair Time of Evaporator, Hours*

*\( \tau_{SD} \) = Repair Time of Steam Drum, Hours*

*\( \tau_{MIN} \) = Minimum Repair Time of any Component, Hours*

*\( A \) = Availability of One Loop, %*

*\( \bar{A} \) = Unavailability of One Loop, %*
the loop. Hence, the success diagram is just one long string of 328 components. Neither the failure mode and effects analysis nor the success diagram is included in this presentation due to space limitation; together they comprise 87 pages of information.

The availability and unavailability of one loop of the steam generator system for various cases, are both tabulated in Table IV. The remarks column summarizes each case. Changes in the availability reflect changes in the failure rates (λ) and the repair times (τ). The unavailability of a component is approximately λτ when this product is in the "neighborhood" of zero for each component, the products may be summed to attain the approximate λτ for the loop unavailability. A discussion of each case follows.

Case 0 was the preliminary case; it corresponds to the 0.75 leaks per year in the heat transfer components of the reference steam generator system. The values input for the failure rates (λ) and repair times (τ) were in general "best estimates" based on engineering judgment.

Case 1 is the base case; it corresponds to the leak rate (49.7/10⁹/tube/hr) reported by Reference 3 for the evaporators and superheaters. This failure rate is about a factor of three greater than the 0.75 leaks per year of Case 0. On the other hand, the downtime for these components was reduced from 1,000 hours to 200 hours. This change was arrived at after discussion with personnel having steam generator operating experience. It assumes that the failed evaporator or superheater will be "blanked off". The unit will be cut out and the open pipes blocked, and the loop would return in 200 hours at about half power, thus total plant power would be 85-90%. In Case 2 updated values for each λ and τ were used to correspond with revised information in the data sheets that was developed from a survey of literature on failure data. Of main concern was the downtime for a loop due to failure of the sodium-water barriers on an evaporator or superheater. Consideration of the possible complexity of valving and/or "blanking off" operations required to return the loop to partial load and the fact that the reference system did not have components in it to permit this approach caused a re-evaluation of the significance of 1,000 hour repair time. Case 2a shows that the change in repair time accounts for nearly all of the added unavailability from Case 1 to Case 2. The effect of updating of λ's and τ's for all other components was minimal in impact on unavailability of system, less than 2%.

Case 3 was evaluated to illustrate the significance of water or steam to sodium leakage in the evaporators and superheaters. Assuming no failures in these units, due to water or steam to sodium leakage, loop unavailability decreased by a factor of 6 (from 9.583% in Case 2 to 1.523% in Case 3).

Case 4 was computed to show the possible importance of steam drum failure rate on the unavailability of the loop. Atomic Intgrnational reported in Reference 3 a steam drum failure rate of 190 failures/10⁶ hours. This was based on three failures in ~17,000 hours operation. Using an estimated steam drum repair time of 200 hours and a repair time of 200 hours for the evaporators and superheaters, as in Case 1, the steam drum unavailability would be more than a factor of eight larger than the unavailability of either of the sodium heat transfer components. Moreover, this change in steam drum failure rate would more than double the loop unavailability of Case 1.

Case 5 is similar to Case 2, but the minimum time to repair any component was increased to 40 hours. Previously this minimum time was assumed to be eight hours. The basis for the eight hour number results from considering the fact that the minimum time to go from 100% to 0 and back to 100% power (or 70% power with two loops) would be eight hours; the basis for this number is the 100°F/hr maximum temperature change in the primary sodium system. Therefore, to shut
down requires at least four hours (1,000°F to 600°F) and equally, return to operation requires four hours (600°F to 1,000°F): the total is eight hours. Instrumentation components were assumed to have this eight hour minimum for repair time, since replacement requires about one hour and may be done on the shutdown time. Other components have less than the 40 hours including non-sodium piping ($\tau = 20$ hours), non-sodium valving ($\tau = 10$ hours), and related components. For Case 5 all of these had their repair times increased to the 40 hour minimum. The basis for the 40 hour repair time is that 40 hours has been considered to be the mean time for shutdown and return to operation in other availability studies (see Reference 4). The results of this change from Case 2 yields a 37% increase in unavailability of the loop.

Case 6 combines the changes made to produce Cases 4 and 5: $\lambda$ Steam Drum = 190/10$^6$ hour in Case 4 based on Reference 3, and $\tau$ minimum = 40 hours in Case 5 based on Reference 4. This combination yielded the highest loop unavailability of all the cases run. Loop unavailability was 66% higher for Case 6 than for Case 2.

Case 7 started with Case 2 as a base and the failure rates for steam/sodium barrier failure, in the evaporators and superheaters, was changed to correspond to the formula reported in Reference 6, F.R. (tube) = $5 \times 10^{-5}$ per tube-hr + $1 \times 10^{-5}$ per ft$^{-2}$ hr. Using this formula the following failure rates are computed: $25 \times 10^{-6}$ per superheater tube - hr and $22.4 \times 10^{-9}$ per evaporator tube - hr. This was used for all 450 tubes in each evaporator as $10.1/10^6$ hours and for 378 tubes in each superheater as $9.5/10^6$ hours. These numbers correspond to about 1 leak/year/system due to Na/H$_2$O barrier failure. The resulting loop unavailability decreased 43% from Case 2.

Case 8 is similar to Case 6 except that the repair time for evaporators and superheaters was returned to 200 hours to conform to the 7-15 days necessary to repair a single tube failure based on Reference 7. The loop unavailability decreased from the high of 15.9% in Case 6 to 9.5% in Case 8, a net decrease of over 40%.

Case 9 is similar to Case 8 except that the failure rates for the evaporators and superheaters were changed to those used in Case 7. This resulted in another decrease in loop unavailability of 8.8% from Case 8. These evaluations show that for the most part the two major components determining loop unavailability are the evaporators and superheaters. Yet if the implications of Reference 3 are considered a third major component, the steam drum must head the list as the single greatest contributor to unavailability.

Table V tabulates the major contributors to loop unavailability. The leaders, the evaporators and superheaters, have been discussed previously. The recirculation pumps have a high unavailability, mainly due to their high failure rate, 28.8 failures/10$^6$ hours (based on Reference 8); this implies about 1/4 failures/year/pump. The sodium pressure monitoring sodium seals have a high unavailability based on their high failure rate of 10 failures/10$^6$ hours, an estimated repair time of 48 hours. The vapor traps for trapping sodium vapor from the cover gas have a high failure rate; 11.4 failures/10$^5$ hour (this value was reported in Reference 8). The pneumatic valves on the sodium drain lines (rank 7) have a composite failure rate. Since the mode of failure on this normally closed valve would be leakage or fails open, both were considered. The first has $\lambda = 2.6 \times 10^{-5}$ hour based on Reference 9 for leakage; the second has $\lambda = 0.22 \times 10^{-5}$ hour based on Reference 10 for valve failing open. The combined failure rate used was $2.22 \times 10^{-5}$ hour. Furthermore, the repair time was considered significant since this is a sodium valve; it was estimated to be 50 hours. The transducers reflect a lack of data in establishment of their failure rate; the failure rate of $24.3 \times 10^{-6}$ hours was reported in Reference 5 and is based on one failure in 41100 hours ($24.3/10^6 = 1/41100$). The generic failure rate for
transducers varies from $4.2 \times 10^6$ hours to $30 \times 10^6$ hour reported in Reference 11. The repair time for failure is the minimum shutdown time of eight hours (40 hours in Cases 5, 6, 8, and 9; see Table IV). It is estimated it would take about one hour to replace the instrument. The Hydraulic oil system has five components, failure of which causes loss of oil system and loop shutdown (the pumps were not considered in the unavailability due to their redundancy). The composite of all failures in this system yields the 1.3% of loop unavailability reported in Table V. The argon gas system was assumed to be on the same order of unavailability. The remainder of 281 components account for less than 19% of the loop unavailability. These include the feedwater piping, steam drum, piping to and from evaporators and superheaters with corresponding valving, instrumentation, etc. Since the valving for the loop consists of various types of valves, of various sizes, and for various fluids, they were not grouped as one into Table V. If they had been, they would have accounted for 10.5% of the loop unavailability and would have been ranked third in the table.

### Table V

**UNAVAILABILITY OF TOP 10 GROUPS OF COMPONENTS OF ONE LOOP STEAM GENERATOR SYSTEM (BASED ON CASE 1)**

<table>
<thead>
<tr>
<th>RANK (NO.)</th>
<th>GROUP (NAME)</th>
<th>TOTAL NUMBER OF COMPONENTS</th>
<th>UNAVAILABILITY (% OF TOTAL)</th>
<th>CUMULATIVE (% OF TOTAL A LOOP)</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>Evaporators</td>
<td>2</td>
<td>29.2</td>
<td>29.2</td>
</tr>
<tr>
<td>2</td>
<td>Superheaters</td>
<td>2</td>
<td>24.6</td>
<td>53.8</td>
</tr>
<tr>
<td>3</td>
<td>Pressure Sensor Sodium Seals</td>
<td>6</td>
<td>9.6</td>
<td>63.4</td>
</tr>
<tr>
<td>4</td>
<td>Rupture Discs</td>
<td>8</td>
<td>5.3</td>
<td>68.7</td>
</tr>
<tr>
<td>5</td>
<td>Recirculation Pumps</td>
<td>2</td>
<td>4.2</td>
<td>72.9</td>
</tr>
<tr>
<td>6</td>
<td>Vapor Traps (Ar)</td>
<td>4</td>
<td>3.0</td>
<td>75.9</td>
</tr>
<tr>
<td>7</td>
<td>Pneumatic Valves (Na)</td>
<td>4</td>
<td>1.5</td>
<td>77.4</td>
</tr>
<tr>
<td>8</td>
<td>Transducers</td>
<td>2</td>
<td>1.3</td>
<td>78.7</td>
</tr>
<tr>
<td>9</td>
<td>Hydraulic Oil System</td>
<td>(15)</td>
<td>1.3</td>
<td>80.0</td>
</tr>
<tr>
<td>10</td>
<td>Argon Gas System</td>
<td>(2)</td>
<td>1.3</td>
<td>81.3</td>
</tr>
<tr>
<td></td>
<td>All Other</td>
<td>(281)</td>
<td>18.7</td>
<td>100.0</td>
</tr>
</tbody>
</table>

Note: If failure rate of steam drum assumed $190 \times 10^6$ hours as in Reference 3, then the steam drum would Rank 1, both as a Component or a Group (see Case 4).

Several groups of components have not been treated as critical in the past with respect to loop (or systems) unavailability. These "new" groups of components include: (1) steam drum and internals, (2) valving, (3) instrumentation, and (4) protection system components. The order implies their approximate significance to loop unavailability. Of course, the evaporators and superheaters will still rank as one and two respectively in the overall picture of loop unavailability.

### REFERENCES


SESSION 15
ACCIDENT ANALYSIS-II
Chairman: G. Fischer (BNL)
CAPRI - A Computer Code for the Analysis of Hypothetical Core Disruptive Accidents in the Predisassembly Phase

D. Struwe, P. Royl, P. Wirtz, B. Kuczera, G. Angerer\textsuperscript{x}, E. A. Fischer\textsuperscript{xx}

Kernforschungszentrum Karlsruhe
Institut für Reaktorentwicklung
\textsuperscript{x}Institut für Neutronenphysik und Reaktortechnik
\textsuperscript{xx}Institut für Angewandte Systemtechnik und Reaktorphysik

Abstract

The newly developed code system CAPRI-2 is described. Results of a parametric study for the predisassembly phase of hypothetical core disruptive accidents for the Mark I-core of the SNR-300 are presented. For the overpower accident the influence of different ramp rates and for the pump flow coast down the influence of different thermohydraulic design data is analysed.

1. Introduction

Two types of hypothetical core disruptive accidents have to be analysed within the licensing procedure for the SNR 300: an unlimited reactivity insertion with a ramp rate up to 5 $$/s and a rapid pump flow coast down. Basic assumption for both accidents is a simultaneous failure of the primary and secondary shut down system. To investigate the predisassembly phase the code system CAPRI has been developed at Karlsruhe. Regarding its structure and objective CAPRI is similar to the SAS-code system [1]. A new version of CAPRI, CAPRI-2, has recently become available. The following describes the actual status of CAPRI-2 and gives results of a parametric study for both accident types.

2. The CAPRI-2 Code

CAPRI-2 is a multichannel code. It is based on a sequential single channel treatment and uses blockwise data transfer with external data pools. The current version of CAPRI is laid out for 30 channels. It is expandable to an arbitrary number of channels without much more core requirement. The code structure of CAPRI-2 is shown in fig. 1. Like the SAS 2A-system [1] CAPRI-2 has four main segments, the input-output segment, the STATO-segment for calculating steady state conditions to initialize the transient, the multichannel driver program RDRIV to organize different modules for the accident simulation, and finally the segment KAINPT to initialize disassembly calculations, which are carried out in a separate step with the KADIS code [2]. All thermohydraulic modules from CAPRI-2 are written for one single channel. They are called through the single channel driver program STAT1 and provided with channel dependent data through the subroutine TIO.
The steady state temperature distribution of fuel, clad, coolant and structure are calculated in STAT0 from the provided input data. Changes of fuel and clad dimensions due to thermal expansion are determined in BREDA and the resulting gap size is consistently iterated with the temperature distribution.

After calculating steady state conditions of the reactor two important accident types, the overpower and the loss of flow accident, can be simulated. The module ITC1 calculates transient fuel and single phase sodium temperatures after accident initiation. Coolant temperatures are determined in a eulerian mesh by the box method. Both fuel and coolant temperatures are calculated simultaneously with one set of equations.

Transient temperatures from ITC1 are used in the BREDA-module [3] to determine thermal loads in pellet and cladding. These loads are taken to calculate pellet and clad radial and axial deformations with a model based on axisymmetric quasistatic plane strain approximation. BREDA results determine the axial expansion reactivity feedback and variations of the gap conductance. Besides that the BREDA module predicts time and position of pin failure in an overpower accident. Failure is determined at time and position where plastic clad deformations and transient clad temperatures exceed threshold values over a prespecified failure rip length. Another failure criterion used in BREDA is based on a threshold for the melt fraction again coupled with a cladding temperature limit.

Fig. 1: The Code Structure of CAPRI-2
Fuel coolant interaction following pin failure is described with the model BRFCI. The model is based on the Cho-Wright formalism but has a more refined treatment of fuel to sodium heat transfer in the two phase region, following the model of Caldarola [4]. The amount of fuel that participates in the FCI is specified at the failure point as fraction of the molten fuel in the cavity. Fuel ejection, fuel motion and condensation effects are not considered.

The relevant CAPRI-2 modules to simulate loss of flow transients are the BLOW 3 module for transient sodium boiling and the SLB module which describes slumping effects. BLOW 3 is a multiple bubble slug ejection model, which has been checked with experiments [5]. It allows a rather detailed treatment of core voiding due to sodium boiling. Boiling starts if the maximum coolant temperature exceeds a channel dependent specified superheat. Evaporation and condensation of a liquid sodium film including subassembly wall effects is taken into account.

Slumping is initiated, when fuel melt fraction and clad temperatures exceed threshold values over a prespecified number of axial zones. Slumping of fuel and clad material is calculated with a simple three region model [6]. Moving of the slumping fuel segment into the free area of the coolant channel underneath the molten zone is described as viscous flow. The pin fragments above the molten zone are assumed to lose their support and fall down towards core midplane.

The reactivity feedbacks from all channels are calculated in the FEEDBK module from channel dependent reactivity worth tables for Doppler, fuel-, clad- and coolant density. They are used in the point kinetics module TSPK [7] to calculate the transient reactor power. The consistency between transient power and reactivity feedbacks is checked. If necessary time steps are repeated so that the transient power results in the same reactivity feedbacks, which were taken for its calculation.

The above mentioned modules represent the current state of CAPRI-2. Additional code development is in progress. A separate module [8], which calculates steady state burnup and restructuring effects, is being coupled to the system. Irradiation effects on transient fuel element behaviour can then be taken into account and the potential of CAPRI-2 to couple up to 30 channels will allow a rather differentiated channel representation regarding various burnup levels in equilibrium core arrangements. A second effort is being made to include a separate clad motion module since fuel and clad motion is described as a coherent process by the current slumping model.

When updated with these models CAPRI-2 will be used for the licensing procedure of the Mark Ia-SNR 300 core. In parallel the different models of CAPRI-2 are to be verified with specifically designed experiments. Of particular importance are the transient overpower tests planned for the CABRI reactor, and multipin boiling experiments as well as out of pile slumping simulations to be carried out at Karlsruhe [9].

3. Results of Parametric Calculations

With CAPRI-2 the fresh Mark I core of the SNR 300 [10] has been investigated. Overpower accidents with different ramp rates have been compared and the influence of different thermohydraulic design data on a rapid pump flow coast down accident has been analysed. The calculations were carried out with a 10- and 26-channel-setup. In the 10-channel-setup the subassembly rings from the
The inner core zone are represented by six channels, those from the outer core zone by three and the radial blanket by one channel respectively. The 26-channel set-up simulated the peripheral power variation in the subassembly rings. The power profiles are shown in fig. 2; BPP 10 represents the base power profile for the 10-channel calculations, PPP 26 the band of peripheral power variations in the different core rings. The maximum peripheral variation occurs in channel 9 with a deviation of ± 8%/o. The radial dependent coolant outlet temperature for the base case are shown in fig. 2 (VTC). The parametric study of the flow coast down uses an alternate power profile (APP 10) and constant coolant outlet temperatures (CTC).

All cases were analysed with reactivity worth curves for fuel, coolant and Doppler based on ref. 11. In this study only coolant density changes, Doppler-and slumping feedbacks were taken into account as feedback mechanisms. Axial
expansion of fuel, clad and structure as well as bowing effects have been neglected. All cases have been analysed up to disassembly. A peak node mean fuel temperature of 3250°C has been chosen as disassembly criterion. Further calculations for the disassembly phase were not carried out.

3.1 Reactivity Insertion Accidents

Overpower accidents with three different ramp rates have been analysed. These are 5 $$/s, 1 $$/s$$ and 50 $$/s$$. For the 1 $$/s$$-case the peripheral power variation was simulated by a 26-channel representation and compared with the base case. With respect to the fresh fuel the following pin failure threshold has been chosen: pin failure in the fresh core takes place, when the average melt fraction in two continuous axial core nodes is higher than 60% while the mean cladding temperature exceeds 750°C simultaneously.

The calculated transient power for the four cases is shown in fig. 2 as function of the FCI-time of the first failing channel. Failure points of the different channels are marked. The differences between the four cases are analysed at four different points of interest. First the core conditions at first pin failure are discussed. They determine the initial conditions of FCI in the central channels, the thermohydraulic state of the reactor relative to the switch over criterion.

Fig. 3: Power Histories of Different Ramp Accidents after Failure of Channel 1
Table 1. Effect of Reactivity Insertion Rates on CAPRI Results for MarkI-SNR 300 Core at first Fuel Pin Failure for constant failure thresholds

Failure Criterion: Two continuous axial nodes have melt fraction > 60 % and mean cladding temperature > 750 °C

<table>
<thead>
<tr>
<th>Design Characteristic</th>
<th>BPP10/VTC</th>
</tr>
</thead>
<tbody>
<tr>
<td>Ramp Rate</td>
<td>$$/s$</td>
</tr>
<tr>
<td>Time of Failure in Channel 1</td>
<td>s</td>
</tr>
<tr>
<td>Normalized Power</td>
<td></td>
</tr>
<tr>
<td>Normalized Energy</td>
<td></td>
</tr>
<tr>
<td>Reactivity: Net</td>
<td></td>
</tr>
<tr>
<td>Programmed</td>
<td></td>
</tr>
<tr>
<td>Doppler</td>
<td></td>
</tr>
<tr>
<td>Void</td>
<td></td>
</tr>
<tr>
<td>Peak Node Mean Fuel Temperature</td>
<td>°C</td>
</tr>
<tr>
<td>Coolant Outlet Temperature in Central Channel</td>
<td>°C</td>
</tr>
<tr>
<td>Fuel Mass Molten Inside the Pin</td>
<td>g</td>
</tr>
<tr>
<td>Failure Position in the Core</td>
<td>cm</td>
</tr>
<tr>
<td>Rip Length</td>
<td>cm</td>
</tr>
</tbody>
</table>

and the neutronics state of the reactor relative to prompt criticality. Secondly the FCI-results of the central channel are investigated to show the influence of different ramp rates on the FCI assuming constant pin failure threshold. Thirdly for the period between first pin failure and prompt criticality the transient power and sequence of pin failure is discussed. Fourth the core conditions at the switch over to disassembly are looked at, especially the voiding pattern, the net reactivity and the associated ramp rates for void and Doppler.

A. The Core Conditions at the Time of First Pin Failure (Table 1)

The highest power at pin failure is calculated in the 5 $$/s$$-ramp and is significantly reduced for smaller ramp rates. The energy input for the 50 $$/s$$ is the highest one. A considerable amount of the produced energy can be removed by the coolant before the failure threshold is met and the coolant outlet temperature becomes much higher. The amount of molten fuel inside the failed pins increases with smaller ramp rates, so does the peak node fuel temperature. In all three cases pin failure is predicted at core midplane.

The net reactivity at failure is close to prompt critical for the 5 $$/s$$ case, it decreases to smaller values with slower ramp rates. Doppler and coolant density feedbacks increase for smaller ramp rates because of higher fuel and coolant temperatures at failure.
Table 2. FCI-Results for central channel of Mark I-SNR 300 core

<table>
<thead>
<tr>
<th>design characteristic</th>
<th>BPP10/VTC</th>
</tr>
</thead>
<tbody>
<tr>
<td>ramp rate</td>
<td>$$/s</td>
</tr>
<tr>
<td>initial fuel temperature</td>
<td>°C</td>
</tr>
<tr>
<td>initial sodium temperature</td>
<td>°C</td>
</tr>
<tr>
<td>fuel to sodium mass ratio</td>
<td></td>
</tr>
<tr>
<td>maximum single phase pressure</td>
<td>atm</td>
</tr>
<tr>
<td>start of sodium vaporization</td>
<td>ms</td>
</tr>
<tr>
<td>maximum two phase pressure</td>
<td>atm</td>
</tr>
<tr>
<td>data at switch-over to disassembly</td>
<td>ms</td>
</tr>
<tr>
<td>FCI-time</td>
<td></td>
</tr>
<tr>
<td>length of FCI-zone</td>
<td>cm</td>
</tr>
<tr>
<td>two phase pressure</td>
<td>atm</td>
</tr>
<tr>
<td>void fraction</td>
<td>%</td>
</tr>
<tr>
<td>fuel temperature</td>
<td>°C</td>
</tr>
<tr>
<td>velocity of upper slug</td>
<td>m/s</td>
</tr>
<tr>
<td>lower slug</td>
<td>m/s</td>
</tr>
</tbody>
</table>

FCI-Parameter: 30 % of the molten fuel inside the pin takes part in FCI mixing time constant $\gamma_m = 5 \times 10^{-3}$ s
particle radius $R_p = 117 \mu$m
time constant of heat transfer in two phase regime $\gamma_B = 0.307$ s

The higher molten fuel temperature at failure and the calculated higher fuel to sodium mass ratio result in more vehement FCI for smaller ramp rates. This is reflected by the higher single and two-phase FCI-zone pressures. The switch over criterion is reached within considerably smaller periods of FCI for high ramp rate cases. Therefore the FCI-results in the central channel differ very much at disassembly. With smaller FCI-periods the FCI-zone cannot expand as much. Consequently, FCI zone pressures remain at high values and the void fraction is still rather small. The mass balance to calculate these void fractions neglects a liquid sodium film remaining on the pin and structure surfaces. Thus up to 95 % void fractions are calculated at core disassembly for large FCI-zones as in the 50 $$/s-case. For the 1 $$/s ramp rate case the differences in the slug velocities show that the upper slug is accelerated faster because of the smaller length relative to the lower slug.

C. Time Dependence of Power up to Prompt-critical (fig. 3)

In a rapid transient only little energy can be removed from the core before failure, so that the different channels fail more coherently. Prompt criticality is
Table 3. Effect of Reactivity Insertion Rates and peripheral power variations in Mark I-SNR 300 Core on Conditions at Disassembly. (Disassembly Criterion $T_{\text{max}}^\text{max} = B$, $m = 3250 \degree \text{C}$)

<table>
<thead>
<tr>
<th>design characteristic</th>
<th>BPP10/VTC</th>
<th>FPP26/VTC</th>
<th>BPP10/VTC</th>
</tr>
</thead>
<tbody>
<tr>
<td>ramp rate</td>
<td>$\frac{s}{s}$</td>
<td>5</td>
<td>1</td>
</tr>
<tr>
<td>time of switch-over</td>
<td>$s$</td>
<td>0.3296</td>
<td>1.4910</td>
</tr>
<tr>
<td>FCI-time</td>
<td>$\text{ms}$</td>
<td>8.98</td>
<td>18.90</td>
</tr>
<tr>
<td>Failure Sequence of Channels</td>
<td>-</td>
<td>1/2/7/3/4/8/5</td>
<td>1/2/7/3/4/8/5</td>
</tr>
<tr>
<td>normalized power</td>
<td>-</td>
<td>1074.7</td>
<td>746.3</td>
</tr>
<tr>
<td>normalized energy</td>
<td>-</td>
<td>5.617</td>
<td>7.216</td>
</tr>
<tr>
<td>energy input</td>
<td>-</td>
<td>1.752</td>
<td>1.325</td>
</tr>
<tr>
<td>during FCI-time</td>
<td>-</td>
<td></td>
<td></td>
</tr>
<tr>
<td>reactivity:net</td>
<td>$\frac{%}{%}$</td>
<td>1.142</td>
<td>1.110</td>
</tr>
<tr>
<td>programmed</td>
<td>$\frac{%}{%}$</td>
<td>1.648</td>
<td>1.491</td>
</tr>
<tr>
<td>Doppler</td>
<td>$\frac{%}{%}$</td>
<td>-0.867</td>
<td>-0.836</td>
</tr>
<tr>
<td>void</td>
<td>$\frac{%}{%}$</td>
<td>0.361</td>
<td>0.455</td>
</tr>
<tr>
<td>ramp rate: net</td>
<td>$\frac{s}{s}$</td>
<td>16</td>
<td>4</td>
</tr>
<tr>
<td>Doppler</td>
<td>$\frac{s}{s}$</td>
<td>-64</td>
<td>-46</td>
</tr>
<tr>
<td>void</td>
<td>$\frac{s}{s}$</td>
<td>80.</td>
<td>49.</td>
</tr>
</tbody>
</table>

reached in about 4 ms in the 5 $\frac{s}{s}$ case. In the 1 $\frac{s}{s}$ case channel 1, 2 and 7 fail farther apart but coherent enough that the power can increase continuously. Compared to the other two cases in the 50 $\frac{s}{s}$-case failure is much more incoherent and transient power goes through a minimum after the inner core zones are voided without failure of further channels. Once prompt criticality is reached the power increases rapidly enough to cause rather coherent pin failures in the outer channels in all investigated cases.

The calculation taking into account the peripheral power variation predicts more differentiated failures with smoother ramp rate variations and earlier power rise due to voiding. On the other hand these variations are not large enough in order to change significantly the behaviour of a fresh core during a ramp rate accident of 1 $\frac{s}{s}$.

D. The Core Conditions at the Switch over to Disassembly (fig. 4, table 3)

The voiding patterns show significant differences for the three ramp rates. For the 5 $\frac{s}{s}$ ramp rate for nearly all channels FCI has been initiated, but according to the very small FCI-time the axial length of the FCI-zones is small. Contrary to that, for the 50 $\frac{s}{s}$-case only the innermost channels have voided.
Fig. 4: Void Patterns at Switch Over to Disassembly in TOP-Accidents
but nearly completely. For the 1 $/s$-case the comparison between the 10- and 26-channel representation shows, that failing only a fraction of the subassembly ring at a time gives a more homogeneous core voiding situation than the 10-channel calculation. In all cases the initial FCI-zone (cross-lined region) for the outer channels are below those in the inner rings, caused by the peak power position in the different channels. It is shifted downwards in the outer core zone due to the control rods.

The comparison of power and net reactivity in table 3 shows maximum values for the 5 $/s$-case with decreasing tendency to the smaller ramp rates. For all cases the reactor is superprompt-critical at switch over to disassembly. The strongest Doppler-feedback is calculated for the 5 $/s$-case because the Doppler-coefficient is least reduced by core voiding. The energy input during FCI reflects the situation at the start of FCI in the central channel. Pin failure in a slower ramp takes place at core conditions which are closer to core disassembly compared to failure in a high ramp rate accident. Thus energy input from FCI is smaller in a small ramp rate accident. The ramp rates at disassembly are very sensitive to only small changes in the time of switch over. One can still see a tendency to smaller voiding ramp rates with smaller ramp rates of reactivity insertion, which is independent of the disassembly criterion. Differences in the void and Doppler reactivity ramps between the 10- and 26-channel representation for the 1 $/s$-case come from temporary local differences in the voiding pattern and fuel temperature distribution at the chosen time of comparison.

3.2 Pump Flow Coast Down

The parametric study for the rapid pump flow coast down accident was concentrated on the influence of different thermohydraulic design data on the accident sequence. Four cases are compared: A. The base case with a 10-channel-set-up of the core and radial dependent coolant outlet temperatures (BPP10/VTC). The smaller coolant outlet temperature in the outermost core ring is of particular importance. It delays boiling in channel 9 with the strongly negative feedback potential. B. The 26-channel-set-up of the core with the peripheral power variation but the same coolant outlet temperature distribution (PPP26/VTC). C. The 10-channel set-up with a constant coolant outlet temperature equivalent to the mean value of the radial dependent distribution (BPP10/CTC). For the same power distribution the constant coolant outlet temperature will diminish the time difference between initiation of boiling in channel 1 and channel 9 compared with the base case. D. The 10-channel-set-up with an alternative power profile and a constant coolant outlet temperature (APP10/CTC). Radial power shape factors of the two distributions are 1.337 for the BPP10-case and 1.400 for the APP10-case. For the same reactor power the peak linear power is about 6% higher than in the base case.

Calculated transient power and reactivities for the different cases are compared in fig. 5 during the boiling and slumping phase up to core disassembly. Important results from these simulations were evaluated with more detail at boiling and slumping and at the disassembly point. Table 4 lists the relevant results for boiling and slumping, table 5 describes the core conditions at the disassembly point. The extent of core voiding at disassembly is compared in fig. 6 for the calculated cases.

For the base case BPP10/VTC sodium boiling starts at 4.47 s with a slightly negative reactivity due to the Doppler-feedback. The subsequent voiding of the
Fig. 5: Power Histories and Reactivity from Boiling and Slumping in Flow Coast Down Accidents (Case Definition see Table 4)
Table 4. Influence of different thermohydraulic design data for the Mark I-SNR 300 core on boiling and slumping during a rapid pump flow coast down accident

Initiation of boiling: $\Delta T_s^1=50^\circ$C; subsequent superheat $\Delta T_s^s=5^\circ$C

Initiation of slumping: Three continuous axial nodes have mean melt fractions $>50\%$ and mean cladding temperatures $>1400^\circ$C

<table>
<thead>
<tr>
<th>design characteristic</th>
<th>BPP10/VTC</th>
<th>FPP26/VTC</th>
<th>BPP10/CTC</th>
<th>APP10/CTC</th>
</tr>
</thead>
<tbody>
<tr>
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<td></td>
<td>1</td>
<td>2</td>
<td>3</td>
</tr>
<tr>
<td>initiation of boiling</td>
<td>s</td>
<td>4.4724</td>
<td></td>
<td>4.6749</td>
</tr>
<tr>
<td>normalized power</td>
<td>-</td>
<td>0.988</td>
<td></td>
<td>0.979</td>
</tr>
<tr>
<td>normalized energy</td>
<td>-</td>
<td>4.492</td>
<td></td>
<td>4.679</td>
</tr>
<tr>
<td>peak node mean fuel temperature</td>
<td>$^\circ$C</td>
<td>1798.</td>
<td></td>
<td>1794.</td>
</tr>
<tr>
<td>sequence of boiling initiation in</td>
<td></td>
<td>1/2.1/2.2/</td>
<td>1/2.3/3.1/</td>
<td>1/2.3/4/</td>
</tr>
<tr>
<td>channel $N^C$</td>
<td></td>
<td>2.3/3.1/4.1/</td>
<td>3.2/3.3/4.2/</td>
<td>7.1/4.3/7.2/</td>
</tr>
<tr>
<td></td>
<td></td>
<td>7.1/4.3/7.2/</td>
<td>7.3/5.1/5.2/</td>
<td>7.3/5.1/5.2/</td>
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<tr>
<td>start of boiling in neg. feedback ch. 9</td>
<td>s</td>
<td>0.7390</td>
<td>0.7293</td>
<td>0.6557</td>
</tr>
<tr>
<td>rel. to ch. 1</td>
<td>$^%$</td>
<td>1.490</td>
<td>1.487</td>
<td>1.582</td>
</tr>
<tr>
<td>maximum void reactivity</td>
<td>$^%$</td>
<td>0.9944</td>
<td>0.9914</td>
<td>1.010</td>
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<tr>
<td>maximum net reactivity before</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>onset of slumping</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>initiation of slumping</td>
<td>s</td>
<td>5.2047</td>
<td>5.1985</td>
<td>5.3345</td>
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<td>time difference to initiation of boiling</td>
<td>s</td>
<td>0.7323</td>
<td>0.7271</td>
<td>0.6595</td>
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<tr>
<td>normalized power</td>
<td>-</td>
<td>138.3</td>
<td>121.4</td>
<td>207.8</td>
</tr>
<tr>
<td>normalized energy</td>
<td>-</td>
<td>7.93</td>
<td>7.90</td>
<td>8.05</td>
</tr>
<tr>
<td>void reactivity ramp</td>
<td>$^%$/s</td>
<td>10.</td>
<td>6.</td>
<td>7.</td>
</tr>
</tbody>
</table>

inner core region creates positive void reactivity feedbacks, which causes significant power rises. Inspite of rapid temperature rises the Doppler feedback is limited at this time through void fraction dependent decreases of the Doppler constant. The rather coherent voiding of the inner core rings due to the flat power profile increases the reactor power to a level which brings the core close to disassembly. The net reactivity reaches nearly prompt criticality. This happens before the outermost channel with a high negative shut down potential can be voided. Slumping takes place, when the average melt fraction in three continuous axial core nodes is higher than 50% while the mean cladding tempera-
ture exceeds the melting temperature of 1400°C simultaneously. When slumping begins the void reactivity ramps are strongly positive. Superposition of these two positive reactivity contributions leads to core disassembly shortly after slumping initiation.

In comparison to the base case the 26-channel-setup does not change the power and void history too much. The relative minima and maxima of power and reactivity during the boiling phase are somewhat smoother due to a more homogeneous change of the voiding pattern. The difference between the two calculations is rather small.

The case with constant coolant outlet temperature shows a later start of boiling in the inner channel and relatively earlier start of boiling in the outer channels compared to the base case. The coolant temperatures are lower in the inner and higher in the outer rings respectively. Besides that for constant power profile the mass flow becomes somewhat higher in the inner while it is somewhat reduced in the outer channels. Delayed but more coherent voiding of the inner core rings contributes higher void reactivity ramps so that prompt criticality can be exceeded for some milliseconds. Due to the resulting sharp power rise the power at initiation of slumping becomes about 50°/o higher than in the base case. But the rapidly increasing Doppler coupled with the negative voiding feedback from the ninth channel reduces the power shortly before the switch over criterion is met.

The larger radial power factor used in the fourth case results in higher steady state mass flow rates for the central channel. Boiling is initiated somewhat later and the voiding sequence becomes less coherent than in the preceding case. The steady state mean fuel temperature of the peak node is nearly 100°C higher due to the higher power density in the central channel. Thus less energy input is required to reach disassembly. The maximum positive void reactivity is about 10°/o lower and the net reactivity remains well below prompt critical. The power at the initiation of slumping is about 90°/o lower than in the preceding case.

The voiding patterns at switch over to disassembly shown in fig. 6 are similar in all four cases, only the voiding of channel 6 and 9 is partly different. The voiding pattern of the 26-channel-setup is more uniform. The cross-lined regions denote the axial positions where slumping has been initiated. Due to different dryout and rewetting sequences for the different channels, the axial position of the slumping region can be below or above core midplane.

Table 5 compares some integral core data at switch over to disassembly. In all cases the very short slumping times and associated slumping reactivities indicate, that slumping has little influence on the predisassembly phase of the different accidents considered. The net reactivity is very close but below prompt critical. For the alternate power profile case (APP10/CTC), the energy input since initiation of boiling is smaller. This is due to the higher mean fuel temperature in the peak node at start of boiling. In all other cases energy input after start of sodium boiling is approximately the same. Comparison of ramp rates shows, that the negative Doppler-ramp nearly compensates the positive void and slumping ramp rates in all cases. Thus the net reactivity increases only very slowly at disassembly, especially if one compares these figures with the ramp rates for the overpower accident.
Fig. 6: Void Patterns at Switch Over to Disassembly in LOF-Accidents

BPP10/VTC

PPP 26/VTC

BPP10/CTC

APP10/CTC
Table 5. Influence of different thermohydraulic design data for Mark I-SNR 300 on core conditions at disassembly

<table>
<thead>
<tr>
<th>design characteristic</th>
<th>BPP10/VTC</th>
<th>PPP26/VTC</th>
<th>BPP10/CTC</th>
<th>APP10/CTC</th>
</tr>
</thead>
<tbody>
<tr>
<td>time of switch-over to disassembly</td>
<td>5.2134</td>
<td>5.2092</td>
<td>5.3440</td>
<td>5.4676</td>
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<tr>
<td>time difference to initiation of boiling</td>
<td>0.7410</td>
<td>0.7368</td>
<td>0.6691</td>
<td>0.7210</td>
</tr>
<tr>
<td>time difference to initiation of slumping</td>
<td>0.0084</td>
<td>0.0107</td>
<td>0.0095</td>
<td>0.0109</td>
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<td>normalized power</td>
<td>-</td>
<td>260.5</td>
<td>187.5</td>
<td>152.</td>
</tr>
<tr>
<td>energy input since initiation of boiling</td>
<td>-</td>
<td>5.114</td>
<td>5.122</td>
<td>5.110</td>
</tr>
<tr>
<td>reactivity: net</td>
<td>0.9993</td>
<td>0.9915</td>
<td>0.9782</td>
<td>0.9892</td>
</tr>
<tr>
<td>Doppler</td>
<td>-0.5896</td>
<td>-0.5884</td>
<td>-0.5970</td>
<td>-0.5672</td>
</tr>
<tr>
<td>void</td>
<td>1.585</td>
<td>1.573</td>
<td>1.572</td>
<td>1.547</td>
</tr>
<tr>
<td>slumping</td>
<td>0.0040</td>
<td>0.0068</td>
<td>0.0054</td>
<td>0.0097</td>
</tr>
<tr>
<td>reactivity ramp: net $/s</td>
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<td>2.</td>
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<td>-6.</td>
<td>-8</td>
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<tr>
<td>void $/s</td>
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<td>7.</td>
<td>7.</td>
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<tr>
<td>slumping $/s</td>
<td>1.</td>
<td>1.5</td>
<td>1.</td>
<td>2.</td>
</tr>
</tbody>
</table>

4. Conclusions

The discussed results of the parametric study with CAPRI-2 are determined to a great extent by the characteristics of the theoretical models used and the assumptions regarding the sequence of events.

The fact that fuel sweep out is not taken into account in the current FCI model causes pessimistic results for the overpower transient, especially for ramp rates lower than 1 $/s$. The results show that FCI zone interfaces leave the core and fuel can be swept out of the core before disassembly conditions are met. Thus consideration of axial motion of fuel could lead to an early shut down of the reactor without disassembly.

All results for the flow coast down accident show that the peak fuel temperatures are brought close to disassembly conditions simply by the rapid power rise during the boiling phase. Thus slumping effects are of minor importance during the predisassembly phase. The main reason for this is the highly positive sodium void worth of the Mark-1 SNR 300. Besides that axial expansion of fuel was neglected. The axial expansion feedbacks strongly reduce power increases during the boiling phase.

Failure or boiling incoherencies due to the assumed peripheral power variations of up to 8° do not cause significant changes in the final results for the accident. At present calculations were carried out for a fresh core alone. Simulation of burnup effects particularly in an equilibrium core arrangement for
which this 30 channel code has been designed is expected to result in rather different accident sequences especially for the overpower accident.

Due to the assumptions of the used theoretical models the calculations for the predisassembly phase appear to be conservative. To improve on this conservative approach the sensitivity of the two types of accidents to the used parameters has to be examined on a broader scale of parametric variations. In addition it becomes necessary to develop more realistic models further validated by experiments.

References


AN IMPROVED ANALYSIS OF FUEL MOTION DURING AN OVERPOWER EXCURSION*

by

H. U. Wider,** J. F. Jackson, L. L. Smith, and D. T. Eggen†

Argonne National Laboratory, Argonne, Illinois 60439

Introduction

During an overpower excursion in a LMFBR, irradiated fuel pins are expected to rupture and eject molten fuel and fission gas into the coolant channels. The ejected fuel is subsequently displaced by the initial sodium momentum, by pressures resulting from fuel-coolant thermal interactions (FCI), and by fission gas released through the clad rupture. This fuel and sodium motion in the core region affects the reactivity of the reactor and determines the ultimate course of the neutronics transient. For example, if the fuel is swept out of the core region, it could potentially terminate the excursion. In this paper the results of PLUTO, a new model for predicting this fuel and sodium motion, are compared with SAS/FCI, a current fuel-coolant interaction model used in whole-core accident analysis. The objective of this comparison is to evaluate assumptions made in current models and to determine the need for more detailed, sophisticated methods.

Models for describing the fuel motion during transient overpower accidents (TOP) have generally used gross assumptions about the pressure distributions in the fuel pin and in the coolant channel. For convenience, these models can be classified as "first generation."

Some of the restrictive assumptions which have been used in the first generation models are:

1) A uniform-pressure fuel/coolant/fission-gas interaction zone in the coolant channel.

2) In some models the sodium above and below the interaction zone is treated as incompressible.2,3

3) The axial fuel motion in the coolant channel is treated in one of the following simplified ways:

   a) The fuel is regarded as uniformly distributed in the expanding interaction zone.4

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*Work performed under the auspices of the United States Atomic Energy Commission.

**Presently at Argonne National Laboratory. Work performed when affiliated with Northwestern University.

†Northwestern University, Evanston, Illinois.

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b) It is assumed that the fuel is distributed according to an arbitrary density skew in the interaction zone.$^5$

c) The fuel in the interaction zone is assumed to move with a velocity which is interpolated from the velocities of the upper and lower boundaries of the interaction zone.$^2$

d) An interpolated velocity, as in c above, is used to calculate the drag forces which move fuel particles where each particle represents all the fragmented fuel from a given failed fuel pin node.$^3$

4) The motion of molten fuel within the pin is treated in the following ways:

a) Fuel is moved only radially into the coolant and no axial motion within the fuel pin is calculated.$^3$

b) During the ejection process, fuel is homogeneously taken out of a uniform-pressure fuel-pin cavity which contains a mixture of molten fuel and fission gas.$^2,^4$

In the new numerical model, PLUTO, the above-mentioned assumptions have been replaced with more detailed modeling of the phenomena. Thus, PLUTO can be regarded as the first of a new generation of fuel-motion models for transient overpower accidents.

Description of PLUTO

In the approach used in PLUTO, the fuel/fission-gas flow in the molten pin region is treated as a homogeneous, compressible, and one-dimensional flow with nonuniform flow cross section. Thus, it was not assumed that there is a uniform pressure in the molten pin cavity nor that the fuel must be uniformly removed from it. The fuel motion is determined by numerically solving a standard set of hydrodynamic equations in Eulerian form. Currently all failed pins in a given subassembly are represented by a single-pin model. However, not all pins have to be assumed to fail. A certain number of statistically distributed pins can remain intact.

As in all present fuel-motion models, PLUTO treats the motion of materials in all coolant channels of a subassembly as a one-dimensional flow. However, this axial flow of materials is treated as a two-component slip flow. The liquid sodium, or the mixture of liquid coolant, vaporized coolant, and fission gas (Na/FG) is regarded as one component and its flow is modeled with compressible, one-dimensional hydrodynamics. The other component is the fuel, which is assumed to be in the form of particles.* The motion of the fuel particles is calculated by solving the momentum equation for representative (or "master") particles.

The momentum conservation for liquid Na, or the Na/FG mixture is solved in the following Lagrangian form.

$$\rho_m \frac{d}{dt} u_m = - \frac{2p}{\eta} \tilde{n}_p (u_m - u_p) |u_m - u_p| \rho_m r^2 \left( \frac{\eta}{2} \right) C_D \varepsilon^{-2.7}$$

$$- \rho_m u_m |u_m| f/(2 D_h) - u_m S_{fg}$$  \hspace{1cm} (1)

*The assumption of the particles is not crucial; it only determines the form of the interactive forces between the two components and the wall friction of the fuel flow.
\[ \rho_m = \rho_m(z,t) \quad = \text{smear density of Na/FG mixture or density of liquid Na} \]
\[ u_m = u_m(z,t) \quad = \text{velocity of Na/FG mixture or liquid Na} \]
\[ p = p(z,t) \quad = \text{pressure in the coolant channel} \]
\[ \bar{n}_p = \bar{n}_p(z,t) \quad = \text{number of fuel particles per cm}^3 \]
\[ u_p = u_p(z,t) \quad = \text{fuel-particle velocity} \]
\[ r_p = r_p(t) \quad = \text{fuel-particle radius (can be reduced after a certain time in order to simulate delayed fragmentation)} \]
\[ C_D = C_D(Re_p) \quad = \text{drag coefficient for a single sphere in an infinite sea of liquid or gas} \]
\[ \varepsilon = \varepsilon(z,t) \quad = \text{non-fuel volume fraction in the coolant channel} \]
\[ f = f(Re) \quad = \text{friction factor} \]
\[ D_h = D_h(z) \quad = \text{hydraulic diameter} \]
\[ S_{fg} = S_{fg}(z,t) \quad = \text{rate of fission-gas injection [g/cm}^3/\text{sec}] \]

The momentum Eq. 1 describes the movement of 1 cc of liquid Na or Na/FG mixture. The second term on the right-hand side models the effect of the particle drag on the Na/FG mixture. The drag law which appears in this term is based on a fluidized-bed correlation.\(^7\) Other drag laws, such as slip correlations for bubbly liquid/gas flow, could also be used.

The momentum conservation for a "Master Particle" is given by

\[ m_p \frac{d}{dt} u_p = - \frac{3p}{\beta z} \left(4/3 \pi r_p^3\right) + (u_m - u_p)\left|u_m - u_p\right| \rho_m \bar{n}_p r_p^2 \frac{2}{c_D} \varepsilon^{-2.7} \]

\[ - m_p g - \left(\pi/4D_h\right) m_p u_p \left|u_m\right| \sqrt{\rho_{fu}/\rho_m} \psi - u_p/n_p S_p \]  \quad (2)

\[ m_p \quad = \text{particle mass} \]
\[ \rho_{fu} \quad = \text{fuel density} \]
\[ \psi \quad = \text{friction factor} \]
\[ n_p = n_p(t) \quad = \text{number of particles represented by one "Master Particle"} \]
\[ S_p = S_p(z,t) \quad = \text{rate of fuel addition to one particle group [g/sec]} \]

The first term on the right-hand side of Eq. 2 represents the sum of perpendicular forces on a particle surface in a pressure field with a linear gradient. The second term gives the frictional drag and the fourth term accounts for the wall friction on a suspension of particles in a pipe. The last term gives the momentum change due to the addition of fuel particles having zero velocity to the group of \(n_p\) particles represented by the master particle.

Due to the numerical treatment of the Na/FG flow with Lagrangian hydrodynamics, the whole coolant channel is covered with a moving mesh which tracks the liquid Na in the coolant slugs and the Na/FG flow in the interaction region.
Frequent rezoning of this mesh is necessary. Each of the Lagrangian mesh cells containing fuel particles is treated as a single interaction zone containing fuel, coolant, and fission gas. This multiple interaction-zone treatment gives an axial resolution to the FCI. It also allows the fuel-coolant heat-transfer coefficient to be space-dependent; e.g., it can depend on the local sodium void fraction. Although sodium-vapor condensation on cold cladding is accounted for in PLUTO, the condensate is currently not assumed to stick to the cold wall but rather to be torn off instantaneously and mixed with the sodium in the coolant channel at the same axial location.

The pressures in the interaction zones are assumed to be the sum of the fission-gas and sodium-vapor pressures. For the fission-gas temperatures, mass-weighted averages between liquid sodium and fuel are used. The calculation of fuel and fission-gas ejection from the pin is based on the assumption that the fuel-pin nodes and coolant nodes adjacent to the cladding-rupture are in pressure equilibrium. The amount of fuel/fission-gas mixture ejected in a time step is set equal to the amount which is necessary to establish the pressure equilibrium. No backflow of materials into the pin is allowed when an FCI leads to a pressure in the channel which is higher than the pin pressure.

Currently, PLUTO is a single-channel, stand-alone code. It does not have a fuel and cladding temperature calculation in the pin, and constant inlet and outlet pressures have to be used. These shortcomings will be resolved by incorporating a version of PLUTO into the SAS code.

Because of its rather general treatment of material motion, other important features could be readily incorporated into PLUTO. Among them is the separate treatment of several pin groups, introduction of a slip-flow treatment of the fuel/fission-gas flow in the pin, treatment of multiple axial pin failures, and, above all, molten fuel freezing to the cladding and channel plugging.

**Description of SAS/FCI**

SAS/FCI is a module of the multichannel SAS3A accident-analysis code. It is one of the more advanced first-generation models for TOP fuel-motion analysis. Within the pins, this model assumes a uniform pressure cavity which contains the molten fuel, the central void gas and the fission gas released from the fuel during the transient. Upon the ejection of the fuel fission-gas mixture through a cladding rupture, the fuel and gas are uniformly taken out of the cavity. The ejection velocity of the mixture is calculated from the time-dependent Bernoulli equation which is obtained by integration along a stream line inside the pin.

The fuel/coolant/fission-gas interaction zone in the coolant channel initially has the length of the cladding rupture and expands after pressurization. Incompressible sodium slugs constrain the interaction zone. Until the lower sodium slug reverses, the lower interaction-zone boundary remains motionless and sodium moves across it. The interaction zone is assumed to be uniform with regard to thermodynamic interaction between fuel and coolant, i.e., it is modeled by one pressure and one temperature for each material component. The heat-transfer coefficient between fuel and coolant can be supplied by the user and the sodium-vapor condensation on the cladding is treated in the same way as in PLUTO. The pressure in the FCI zone is first determined by the fission gas (adiabatic behavior assumed) and the compressibility of the liquid sodium. After the sodium-vapor pressure exceeds the fission-gas pressure the interaction zone pressure is equal to the sodium vapor pressure.

Although it is assumed that fuel and coolant are uniformly distributed in the interaction zone for the FCI calculation, a more realistic fuel
Fig. 1  Comparison of PLUTO and SAS/FCI Model Structures
distribution is computed for use in the reactivity calculation. This is done by assuming that the fuel at a given location in the channel moves with a velocity which is interpolated from the velocities of the upper and lower interfaces of the interaction zone. In the code, uniformly spaced cells in the interaction zone move with these interpolated velocities. If the fuel cells become too large, they are rezoned. Fuel is injected only into those cells positioned in front of the rupture and then remains in those cells.

Since this paper compares PLUTO with SAS/FCI, the major differences between these models are highlighted in Table 1 and Fig. 1.

Table 1. Differences Between SAS/FCI and PLUTO

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<tr>
<th></th>
<th>SAS/FCI</th>
<th>PLUTO</th>
</tr>
</thead>
<tbody>
<tr>
<td>Fuel/fission-gas</td>
<td>Uniform material removal from pin cavity</td>
<td>Solves compressible hydrodynamics eqs.</td>
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<tr>
<td>motion in pin:</td>
<td></td>
<td></td>
</tr>
<tr>
<td>FCI treatment in</td>
<td>Single FCI zone</td>
<td>Multiple FCI zones (Lagrangian treatment of Na/FG flow)</td>
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<tr>
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<tr>
<td>Fuel motion in</td>
<td>Fuel velocities interpolated from FCI zone boundary velocities</td>
<td>Solves momentum equation</td>
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<tr>
<td>Coolant motion</td>
<td>Incompressible slugs assumed</td>
<td>Solves compressible hydrodynamics eqs.</td>
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<td>outside FCI region:</td>
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</tbody>
</table>

Single-Channel Comparison Calculations

A central FFTF driver subassembly with 100 days of burnup was considered for the comparison calculations. Coherent failure of all pins was assumed to occur at the time of 50% areal melting at the midplane. The failure location was assumed to be at 3/4 of the core height and 5 cm was used for the length of the rupture.

The SAS3A program was used to predict fuel pin failure conditions. The input power history resembled a 50°C/sec reactivity insertion. At the time of clad failure SAS3A predicted 42.5 g of molten fuel above liquidus and 0.032 g of free fission gas per pin. This corresponds to a pin pressure of 300 atm at the time of failure. The sodium flow velocity at failure time was 800 cm/sec. Inlet and outlet pressures of 8.25 atm and 1.35 atm, respectively, were used. In this case, it was assumed that no fission gas was released prior to the ejection of the homogeneous fuel/fission-gas mixture. The following heat-transfer coefficients between fuel and sodium were used in both models:

\[ h = \left( \frac{k_f}{r_p} \right) \left[ 1 - \alpha(z,t) \right] \]  \hspace{1cm} (3)

where \( k_f \) is the fuel conductivity, \( r_p \) is the fuel particle radius, and \( \alpha(z,t) \) is the sodium void fraction (in SAS/FCI \( \alpha \) depends only on \( t \)). Equation 3 is a modified form of the steady-state heat transfer coefficient given by Cho.
The factor \([1 - \alpha (z,t)]\) brings about that only the fraction \([1 - \alpha (z,t)]\) of the fuel at location \(z\) and time \(t\) transfer heat to the sodium.

Results will be compared for the following three cases:

<table>
<thead>
<tr>
<th></th>
<th>Case A</th>
<th>Case B</th>
<th>Case C</th>
</tr>
</thead>
<tbody>
<tr>
<td>Fuel-particle radius</td>
<td>0.025 cm</td>
<td>0.025 cm</td>
<td>0.010 cm</td>
</tr>
<tr>
<td>Pin failure pressure</td>
<td>300 atm</td>
<td>100 atm</td>
<td>300 atm</td>
</tr>
</tbody>
</table>

In Case B it was assumed that only 1/3 of the amount of fission gas predicted by SAS2B was released from the fuel.

**Nominal Case (Case A)**

In this case best estimates for the various parameters were used.

Figure A1 shows the pressure and velocity profiles in the coolant channel at 1 msec after cladding failure. These are PLUTO results; SAS/FCI is not designed for calculating these compressible effects. The shape of the two pressure waves moving up and down the channel reflects the early pressure-time history in front of the rupture which is given in Fig. A2. The sodium through which the pressure wave has already traveled has gained velocity if it is downstream from the rupture, or lost velocity if it is upstream. Once the pressure waves have reached the inlet or outlet (which are assumed to be free surfaces in PLUTO), rarefaction waves move toward the rupture. After the rarefaction waves have reached the interaction region, new pressure waves are generated. Pressure pulses with periods similar to those predicted by PLUTO have been measured on the pressure transducers for TREAT experiments E4, E6, H4, and H5.9

In interpreting Fig. A2, it should be noted that the PLUTO pressure trace refers to the location directly in front of the rupture, whereas the SAS/FCI result is an average interaction-zone pressure. The pressure trace predicted by PLUTO drops abruptly at around 2 msec. This is due to the arrival of the first rarefaction wave from the inlet, which relieves the high pressure. Between 5 and 10 msec, a mild fuel-coolant interaction (FCI) keeps the pressure up. The SAS/FCI pressure starts at the initial channel pressure. This is more reasonable than the initial pressure in PLUTO which is based on equilibrium between the nodes behind and in front of the rupture. However, the SAS/FCI pressure never becomes as high as the PLUTO pressure during the first 10 msec. This is due to the fact that less fuel and fission gas are ejected during the first milliseconds in SAS/FCI (see Fig. A3). Moreover, fission-gas pressure and sodium-vapor pressure are not summed in SAS/FCI. The FCI predicted by SAS/FCI occurs later and is considerably stronger than in PLUTO. The delay occurs because the fuel has to heat all the sodium in the interaction zone in SAS/FCI. In PLUTO the fission-gas pressure tends to quickly separate some liquid sodium from the fuel, and thus less sodium is heated by the fuel. The FCI in PLUTO is self limiting since the sodium-vapor pressure also separates liquid sodium from the fuel. This explains why the FCI predicted by PLUTO is considerably weaker than the one in SAS/FCI. (Maximum Na temperature in PLUTO 1302°C vs 1616°C in SAS/FCI). It should be noted, however, that the FCI results predicted by PLUTO are strongly affected by the void fraction dependence in the heat-transfer coefficient (Eq. 3). Nevertheless, it seems to be physically reasonable that an FCI will separate the liquid sodium from the fuel if the constraints on the sodium are weak. Moreover, such a separation of fuel and sodium will limit the FCI.
Fig. A1  Pressure and Velocity Distributions in Coolant Channel

Fig. A2  Pressure-Time Histories in Front of the Rupture (PLUTO) and in the Single FCI Zone (SAS/FCI)

Fig. A3  Total Mass of Fuel in the Coolant Channel
Figure A3 shows the amount of fuel ejected from one pin as a function of time. During the first 3 msec, SAS/FCI predicts a considerably lower rate of fuel ejection than PLUTO. This is due to the fact that a rupture area equal to the cross-section area of the pin cavity at the rupture location (= 0.1 cm²) has been used in SAS/FCI. The fuel/fission-gas mixture right behind the rupture is, therefore, not ejected as rapidly as in PLUTO. To use a larger rupture area in SAS/FCI is unrealistic since the expulsion is mainly controlled by the small pin cavity diameter. The larger ejection rates predicted by SAS/FCI after 50 msec are due to the lower pressure in the FCI zone. The FCI zone has become considerably larger than the FCI region in PLUTO by 50 msec (see Fig. A8).

Figure A4 shows pressure, density, and velocity distributions in the pin cavity at 13 msec as calculated by PLUTO. It can be seen that the flat density profile supports the SAS/FCI assumption that the fuel can be uniformly removed from the pin cavity. The pressure profile away from the rupture is also fairly flat by 13 msec. This is not the case in the first few milliseconds after failure when rarefaction waves move from the rupture location to the ends of the molten pin cavity.

Figure A5 depicts pressure and temperature profiles in the coolant channel at 13 msec as calculated by PLUTO. The high sodium temperatures indicate the location and length of the interaction region. Below this region there is an expansion situation in the liquid at this time. Above it is a compressive situation. The dip in the pressure wave is caused by the decreasing pressure in the expanding FCI region. The pressure profile inside the FCI region is peaked at the rupture location. This is because of the large amount of fuel in front of the rupture (see Fig. A6) which impedes the flow of fission gas and fuel from the rupture. Away from the rupture the pressure profile in the FCI region is relatively flat. This is due to the higher sodium vapor pressures and the higher sodium temperatures at the ends of the FCI region than in the center of the FCI region.

To have hotter sodium at the ends of the FCI region than in the center seems paradoxical, but it results from the separation of the liquid sodium from the bulk of the hot fuel. The heated liquid sodium thus accumulates at the ends of the FCI region (see Figs. A6, A7, and A8). Since a large amount of hot sodium has a larger heat content than a small amount, expansion and condensation cool the sodium at the ends of the FCI region more slowly than the small amount in the center. Therefore, the sodium temperatures at the ends of the FCI region are higher than in the center during the cooling period.

Figures A6, A7, and A8 show fuel distributions in the coolant channel at different times after failure as calculated by SAS/FCI and PLUTO. Also shown is the sodium void-fraction distribution predicted by PLUTO. The void fraction in the SAS/FCI calculation is, of course, uniform in the single FCI zone. The interaction zone in the SAS/FCI calculation expands faster due to the stronger FCI calculated by this code. The PLUTO results show that most of the liquid sodium in the FCI zone soon separates from the bulk of the fuel because the light Na/FG mixture slips by the heavy fuel particles. The fuel distributions predicted by both codes are strongly peaked at the rupture location, but there are additional peaks in the PLUTO fuel distributions. The peaks at the ends of the FCI region indicate that fuel approaching the liquid-coolant slugs has slowed down and piled up. The peak below the uppermost one in Figs. A7 and A8 is due to the early deceleration of some fuel by the elevated sodium vapor pressures above the rupture. In general, such piling up is not unusual in particle flows.

PLUTO predicts more upward than downward fuel motion from the rupture due to the strong pressure gradient above the rupture during the first 20 msec.
Fig. A4 Density, Velocity, and Pressure Profile in the Molten Pin Region

Fig. A5 Pressure and Temperature Profiles in the Coolant Channel

Fig. A6 Fuel and Na Void Distributions in the Coolant Channel
Fig. A7 Fuel and Na Void Distributions in the Coolant Channel

Fig. A8 Fuel and Coolant Distributions in the Coolant Channel

Fig. B1 Fuel and Na Void Distributions in the Coolant Channel
SAS/FCI predicts stronger downward movement than PLUTO. This is because there was little downward displacement of the lower interface during the first 10 msec when the pressure in the FCI zone was low. Once the lower interface accelerates due to the FCI, the point of zero velocity as interpolated from the FCI zone interfaces is above the rupture. Therefore, any fuel ejected after this time must be moved downward. This effect is more pronounced in the next case.

Decreased Pin-failure Pressure (Case B)

Figure B1 shows fuel and sodium void distributions in the coolant channel in a case in which only 1/3 of the fission gas in Case A was assumed to be released from the fuel. This led to a failure pressure of only 100 atm. Due to the decreased pin pressure, the lower FCI zone interface in the SAS/FCI does not move downward for 20 msec. Most of the fuel is "pulled" downward later by the lower boundary when the FCI pressure becomes high. In the PLUTO calculation the lower FCI zone boundary is not moved downward as much due to a weaker initial FCI. By 40 msec the lower slug has already returned and a mild FCI begins at the lower end of the FCI region which pushes all the fuel upward. This PLUTO prediction seems to be physically reasonable because an FCI can only occur where fuel and coolant are together and because a pressurization at the lower end of the FCI zone should lead to upward fuel motion.

In a different case, in which only failure of half of the pins was assumed (failure pressure 300 atm), SAS/FCI also predicted much more downward motion than PLUTO. In the SAS/FCI calculation this was again due to a late reversal of the lower FCI-zone boundary.

Stronger FCI (Case C)

Since the FCI had only a weak influence on the nominal PLUTO case, a stronger FCI was assumed in Case C by using a fuel-particle radius of only 0.01 cm.

Figure C1 shows that the FCI predicted by SAS/FCI is again considerably stronger and occurs later than the FCI predicted by PLUTO (cp. Fig. A2). In Fig. C2 pressure and temperature profiles in the coolant channel as calculated by PLUTO are depicted. It can be seen that the sodium temperature and pressure are considerably higher in the upper part of the FCI region than in the lower one. There are two reasons for this. Initially the small 0.01 cm fuel particles are swept upward quite fast. Thus, they contact the sodium in the upper FCI zone region for a relatively long time and substantially heat it. Moreover, there is more liquid sodium in the upper part of the FCI region than in the lower one. Thus, it cools relatively slower than the sodium at the lower end. It was already discussed earlier (see Fig. A4) that the sodium in the center of the FCI zone cools much faster than the sodium at the ends.

Figure C3 shows the effect of the pressure profile in the interaction zone on the fuel distribution as calculated by PLUTO. Considerable down-stream movement of fuel is predicted by PLUTO in this case. The downward movement calculated by SAS/FCI is much less. SAS/FCI also predicts less downward movement than in Case A in which the FCI was weaker (cp. Fig. A8). This is due to the fact that the lower interaction-zone boundary is accelerated downward earlier in Case C than in Case A due to the earlier FCI pressure. Thus, not as much fuel is "pulled" downward by the FCI-zone boundary.

The fuel downward motion as predicted by PLUTO is particularly important in cases in which pin failure at the reactor midplane is assumed. Due to this effect, the fuel remains in the active core longer and the overall fuel and sodium motion feedback can remain high for up to 50 msec in such cases.
Fig. C1 Pressure-Time Histories in Front of the Rupture (PLUTO) and in the single FCI zone (SAS/FCI)

Fig. C2 Pressure and Temperature Distributions in the Coolant Channel

Fig. C3 Fuel and Na Void Distributions in the Coolant Channel
Conclusions

The objective of this paper was to evaluate some of the main simplifying assumptions used in current models and to determine the need for more sophisticated methods. By comparing SAS/FCI with PLUTO, most of the restrictive assumptions mentioned in the introduction could be directly checked. Conclusions about other assumptions made in the first-generation models can also be drawn from this comparison.

It was found that the simplified treatment of the fuel/fission-gas flow in the pin which is used in SAS/FCI and in Ref. 4 is appropriate. However, the PLUTO results indicate that, except for the fuel ejected initially from right behind the rupture, the fuel ejection is controlled mainly by the cavity cross-sectional area. Since the SAS/FCI model presently allows only one time-independent flow area, the use of such a flow area in the model leads to too little fuel ejection during the first few milliseconds after failure.

The fuel-coolant interaction in a single-FCI-zone model is stronger and occurs later than in the more detailed multiple-interaction-zone treatment. This is due to the fact that all fuel interacts with all sodium in a single FCI zone. The strong separation of fuel and coolant as predicted by PLUTO cannot occur in a single-interaction-zone treatment.

The fuel motions in the coolant channel predicted by PLUTO and by SAS/FCI differ greatly in some cases. In cases in which the lower FCI-zone interface does not reverse for a relatively long time, SAS/FCI shows a strong downward movement of the fuel which does not seem to be physically reasonable. On the other hand, SAS/FCI does not predict as strong a downward fuel motion as PLUTO for the case of a relatively strong FCI. This is because SAS/FCI does not calculate the pressure gradients in the FCI region. In general, the strong coupling of the fuel motion with the FCI zone boundaries used in SAS/FCI and Ref. 3 is not supported by the PLUTO results. Moreover, disregarding the pressure gradients and the fuel - Na/FG slip in the interaction region cannot be justified. The use of an arbitrary fuel-distribution function, even if peaked at the rupture, also appears to be inadequate since the fuel distribution depends upon the case being calculated.

PLUTO results indicate that the sodium columns outside the FCI region behave relatively incompressible beyond 10 msec after pin failure. Although the comparison did not indicate whether the compressible treatment of the liquid-sodium columns is really necessary for whole-core analysis, the compressible treatment is certainly useful for analyzing experiments in which the pressure history is obtained from pressure transducers.

In general it seems fair to say that there is a definite need for a more sophisticated method, such as used in PLUTO, to treat the fuel motion in the coolant channel. One could probably use simplified methods such as used in SAS/FCI for treating the fuel/fission-gas flow in the pin cavity and to assume incompressible liquid sodium outside the FCI zone. However, a code like PLUTO which uses more detailed modeling in the pin and in the whole coolant channel is more versatile. This is because it can be readily improved by including slip flow in the pin, treatment of multiple axial failures and fuel freezing to cold structures.

A code such as PLUTO which uses more detailed modeling allows more insight into the phenomenology of the transient-overpower accident. For example, PLUTO calculations presented in this paper show that an in-core fuel-coolant interaction is self-limiting because high sodium-vapor pressures separate the liquid coolant from the fuel and push the hot coolant toward the ends of the interaction region. Another example is that the extensive fuel freezing and channel plugging found in postmortem investigations of some experiments can
be better understood from the local high fuel concentrations in the coolant channels calculated with PLUTO. On the other hand, many effects calculated by PLUTO can only be validated by detailed comparison with carefully done experiments. It should be noted, however, that the fuel motion during an overpower-transient situation cannot be quantitatively measured at present.

Finally, a brief comment on the economics of running PLUTO on the computer might be appropriate. PLUTO requires about 3 min of IBM-195 CPU time to execute a typical single-channel problem out to 100 msec. SAS/FCI can run the same problem in about 30 sec. Although the detailed hydrodynamic treatment in PLUTO obviously requires more time, the difference is not excessive.

Acknowledgment

We would like to acknowledge the many valuable contributions made by Dr. R. B. Nicholson during the development of the PLUTO fuel motion model.

References


FUEL MOVEMENT INVESTIGATIONS DURING LMFBR OVERPOWER EXCursions USING A NEW MODEL

E. T. Rumble III*, W. E. Kastenberg†, D. Okrent++, and J. O. Cermak**
University of California, Los Angeles

ABSTRACT

Several physical models approximated in a computer simulation of the predisassembly phase of LMFBR overpower excursions are described. Results are presented showing clad failure predictions, fuel movement and related phenomena.

1. INTRODUCTION

The object of this paper is to discuss results obtained from the computer code, HOPE (Hypothetical Overpower Excursion), an integrated model for studying whole core or single pin predisassembly behavior. Attention is devoted to new areas of study incorporated in the model. These include: 1) Clad failure time and location predictions based on permanent deformation; 2) Fission heating of fuel particles which exist in the coolant channel; and 3) Space and time dependent fuel fragmentation. Further information about HOPE will appear in Reference 1.

2. PHYSICAL MODEL

A. General

Phenomena treated by this computer model for studying fuel movement include: reactor kinetics; steady state fuel behavior; steady state and transient fuel, clad and single phase coolant heat transfer; fuel melting and formation of a molten fuel-fission gas cavity; clad deformation and failure; fuel and fission gas ejection; fuel fragmentation and interaction with the coolant; fuel movement and reactivity feedbacks. The model is applicable to all burnup and operating power conditions of either LMFBR whole cores or single pins in which sodium coolant boiling does not occur prior to clad failure.

The fundamental unit described in HOPE is a fuel pin with its associated coolant. All fuel pins in one subassembly are assumed to act coherently. Thus, a subassembly model includes a representative fuel pin, its coolant and its associated subassembly jacket. Whole core excursions are modeled by describing the core as a collection of representative subassemblies or channels. Interactions between channels are not modeled as the subassembly jackets are assumed to maintain their integrity and the core transient thermal and hydraulic boundary conditions are fixed.

B. Steady State

Initially, the state of the reactor is determined from specified input conditions. Discrete fuel, clad, coolant and subassembly jacket temperatures and other properties are found for a specified radial-axial node mesh. A fuel restructuring and center void formation model is employed for those fuel

*Student, †Co-Advisors, **On leave from Westinghouse Electric Corporation.
pins capable of restructuring. As one of several possible options, the amount of fission gas released by the fuel is modeled using Reference 2. The center void pressure is determined by assuming pressure equilibrium with the gas plenum. Although the flow rate between this plenum and the center void may normally be very small, equilibrium is assumed to exist because of long periods of steady state operation and temporary fuel cracks formed during shutdowns.

C. Pre-Clad Failure Transient

The radial and time dependent quasilinear parabolic heat conduction equation is solved in a straightforward manner\(^3\) to find the fuel and clad temperatures, spatially and temporally discretized, in a radial-axial-time node mesh. A point kinetics routine, in which doppler, sodium voiding and fuel movement feedbacks are included, specifies the heat source term. The single phase continuity, momentum and energy equations are used to determine the time and axially discretized coolant conditions. A simplified fuel-clad thermal expansion model is employed in which the clad is considered to behave as a thin membrane and the solid fuel is allowed to transmit stress to the clad assuming no slip between them. Plastic deformation of the clad is described using a high strain rate, irradiated, temperature dependent yield surface.

The fuel pin cavity is presently assumed to be homogeneous and impermeable during this phase. Its size and state is determined by the combination of the center void, molten fuel, released fission gas and initial central void fission gas. A check on the degree of homogeneity is performed by calculating the density of molten fuel in each axial node of the cavity assuming no fuel motion in the cavity.

The fuel cavity pressure is found by an equation of state, which for a given cavity temperature, determines the pressure-volume relationship for the mixture of central void fission gas, released fission gas of a specified bubble size and liquid fuel. The volume available for the cavity is found at each time step by calculating the melt radius of each axial node. The Berthelot equation of state\(^4\) for a gas has been employed in this formulation.

The pressure in the fuel pin cavity is transmitted through the solid fuel to the clad. The clad is assumed to be a thin membrane and the solid fuel is considered strengthless. Thus the pressure on the clad exerted by the cavity is a function of the ratio of the fuel cavity outer radius to clad inner radius. When the cavity induced stress exceeds that caused by differential fuel-clad thermal expansion, the clad stress is assumed to be caused entirely by the cavity pressure. This is necessary to avoid use of a contradictory description of the fuel's strength.

Clad failure time and location is predicted by setting a clad permanent deformation or stress failure condition. For flexibility, the user can also specify the failure location at any position in the fuel pin. In addition, numerous other failure time criteria can be selected. The clad failure flow area is specified initially for each channel being studied. No allowance is presently made for multiple failures or a time dependent failure area.

D. Fuel and Fission Gas Ejection

The time dependent Bernoulli equation with additional flow choking considerations is employed to model the assumed homogeneous, compressible ejection of fuel and fission gas. This treatment is similar to that found in Reference 5. Consideration for the initial ejection of the solid fuel behind the clad failure is made. In addition, the possibility of the fuel-fission gas flow being limited in the fuel pin cavity is considered.

E. Fuel Fragmentation

Although a firm theory does not exist at present to explain
fragmentation, experimental observations gives clues about its characteristics. Investigators have shown that for molten tin and water experiments in which the water is above a bulk temperature of 70°C, no interaction occurs. Through a mechanical disturbance, however, the film preventing liquid-liquid contact is broken and fragmentation occurs. As the water temperature is further increased, it becomes more difficult to induce fragmentation by disturbing the molten tin, and near saturation (95°C for water) fragmentation does not occur, even with mechanical disturbance. Other experiments performed with different materials follow these general characteristics, although specific evidence has not been obtained for the case of sodium and molten uranium dioxide establishing a cut off state. It is assumed in this work that for the reactor situation, the molten fuel will be highly disturbed and fragmentation will not occur if the bulk sodium is less than a few degrees subcooled or the particle is not molten. In addition, for the case in which a significant quantity of fission gas is ejected, fragmentation can be stopped at a prespecified void fraction considering homogeneous distribution of the fission gas and sodium vapor in the interaction zone.

When fragmentation is permitted, the mass of fuel particles is divided exponentially during a specified time interval (obtained from experiments) into more particles of smaller volume. A number of mass weighted particle size distributions are selected from experimental information to describe this time dependent increase in effective heat transfer area. A heat transfer model using the effective heat transfer area obtained from this fragmentation model will be discussed in the next section.

As the fuel is ejected from the clad failure area, small discrete fragmentation groups are established. These represent the mass of fuel ejected during the delay time before fragmentation begins as has been observed in experiments. With the establishment of the series of fragmentation groups which in summation contain all the fuel mass in the coolant, these particles can be temporally followed to study the effects of independent variable group fragmentation.

F. Fuel-Coolant Interaction
A single zone interaction model is employed in which the length of the zone is initially specified as the clad failure length. This model, similar to that discussed in Reference 5, assumes a uniform distribution of the fuel in the zone (the zone bounds all the ejected fuel) and gives only the average state of the sodium within it.

An energy equation is employed in which the heat transferred between the clad, upper and lower zone interfaces and fuel particles is considered. An equation of state for either single, two or three phase conditions (sodium liquid, vapor and fission gas) and an inertial constraint equation are coupled with the energy equation and solved by Gauss elimination to find the average state of the coolant. These properties are then used to control fragmentation, calculate the fuel particle and contacting fuel pin temperatures, determine the coolant conditions outside of the interaction zone, and calculate forces on the fuel particles.

Clad heat transfer is described by using a condensation coefficient when the clad is cooler than any existing sodium vapor and a Nusselt number correlation otherwise, such as in Reference 9. Heat transfer between the fuel particles and sodium takes into account the fuel's thermal conductivity and convection heat transfer to the sodium coolant based on the use of a Nusselt number correlation. Implicit in this model is the assumption that the fragmentation process itself is not energetic as discussed in Reference 10. The state of the coolant is controlled only by the energy transfer with the interaction zone, its characteristic state equation and the assumed incompressible
sodium constraining columns above and below it.

G. Fuel Movement in the Coolant Channel
One difficulty in describing the movement of coolant channel fuel particles occurs because it is impractical to explicitly describe each particle. Instead, all the particles, or a number of representative groups of them, must be bounded in cells. The movement of the cell's upper and lower boundaries will then describe the extent to which the particles within the cells axially dilate.

In this work, for purposes of describing the particle movement, the fuel ejected into the coolant is divided into $M \times N \times L$ independent cells where $M$ = the number of fragmentation groups, $N$ = the number of representative particle sizes for each fragmentation group and $L$ = the number of initially axially connected equal volume cells that together equal the volume of coolant associated with the failure length. There will be, then, $M \times N$ aggregations of $L$ cells, where each $L$ cell aggregation contains only one particle size of one fragmentation group.

Considering one cell, the fuel mass within is represented by a uniform dispersion of particles all with the same time dependent (due to fragmentation) radius. The motion of the cell is followed by calculating the upper and lower cell boundary translation, assuming the same type and density of particles on and near its boundaries. The movement of the particles is determined by using the assumed axially, linearly variable, coolant interaction zone, sodium velocity and calculating the drag, gravity and buoyancy forces on the particles. A correction for the situation in which the particles become closely packed is also included.

A further calculation is necessary to transfer the position of the fuel particles at any given time back to an Eulerian mesh for reactivity and information purposes. Fuel mass distributions as function of particle size and axial position are defined in the output of the program.

3. RESULTS
A. Input
The SNR-300 LMFBR system with a thermal power rating of 730 MW is analyzed. The reactor core is about 158 cm in diameter and consists of two zones of different enrichment. It contains 151 subassemblies, each holding 169 fuel pins filled with PuO$_2$-UO$_2$ pellets. Each fuel pin has an initial outer diameter of about .6 cm and is approximately 245 cm long (96 cm of active fuel, 80 cm of axial blanket, 65 cm of lower plenum and 4 cm of upper gas space). The sodium coolant which flows upward through the core, represents about 50% of the total core volume and has nominal steady state full power inlet and outlet temperatures of 377°C and 546°C.

Whole core studies are analyzed by using 10 representative channels, 6 to represent the first enrichment zone, 3 for the second zone and 1 for the radial blanket. Each channel's representative fuel pin, associated coolant and subassembly jacket is described by an axial-radial node mesh. The active core fuel is located between height 40 cm to 136 cm (bottom of lower blanket called 0 cm) and is described by a 10 axial (axial node numbers 3 to 12) and 10 radial node mesh.

B. Single Pin Failure Study
Using a single pin model representative of an SNR-300 fuel pin, a failure state sensitivity study was performed. Results were obtained as a function of the input reactivity ramp rate and the pins operating power and burnup. Failure is predicted when an axial clad node reaches the ultimate
stress condition. Irradiated, high strain rate, temperature dependent properties of 20% cold worked 316 stainless steel are used for the clad.

The study indicates that for the assumptions made in the model the failure location is fairly insensitive to burnup, but is sensitive to the pin's operating power. Higher power pins fail higher in the core and low power pins fail near the core midplane. This is caused by the interplay between the cavity pressure, volume of the cavity and temperature dependent clad strength. Because of increased fission gas retention, the lower the operating power, the larger the cavity pressure, for a given cavity volume. In low power pins, very little molten fuel is present at the time the internal pin pressure is able to fail the clad. Thus, the low power pin cavity stresses and fails the clad at the same elevation (most active portion of the fuel pin, normally at or near the midplane) in which the cavity is located. Higher power pins will transmit stress to a longer segment of the clad and thus weaker portions of the clad will be susceptible to failure. Failure locations in this model are dependent on the cavity pressure model. Inclusion of a solid fuel fission gas release model, for example, might cause the clad to fail at a lower position for a given operating power.

For cases 1, 2 and 9 in Table I, in which only the input reactivity ramp rate is varied, agreement with trends indicated in Reference 13 is obtained. To check the homogeneity of the cavity at failure, considering no fuel movement within, the axially dependent density of molten fuel in the cavity (mass of molten fuel in one axial cavity node divided by available volume in that axial node) is calculated and shown in Table I. The variations generally show lower densities in the cavity center. Case 1 shows the largest axial variations because of the fuel's larger departure from adiabatic behavior. These variations would be reduced if axial heat conduction were included.

C. Single Pin Transient Studies

A series of studies using a single fuel pin of SNR-300 have been conducted. The reactivity worths are set as shown below ($1 = 3.04 \times 10^{-3} \Delta \rho$).

<table>
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<th>Reactivity Effect</th>
<th>Peak Axial Node Worth ($)</th>
<th>Channel Worth ($)</th>
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<tr>
<td>Doppler (sodium in)</td>
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<td>-1.85</td>
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<td>Doppler (sodium out)</td>
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<td>Sodium Voiding</td>
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<tr>
<td>Fuel Removal</td>
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</table>

Sodium voiding and fuel motion reactivity effects have been reduced to provide a study less dependent on variations in power. The single pin studies shown in Figures 1 through 6 are presented below.

<table>
<thead>
<tr>
<th>Fig.</th>
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<th>B.U. (at %)</th>
<th>Ramp ($/sec)</th>
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<td>base(^2)</td>
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\(^1\)All particles ejected with a 125 micron radius.

\(^2\)Fragmentation time = 5 msec., 3 group final particle distribution for groups 1/2/3; radius = .5/.125/.0625 (of original radius); weight = .3/.5/.2 (of total weight)
The figures do not show initial portions of the transient, but rather focus on details just before and after failure. The transients have been stopped when the hottest fuel node reaches 6000°K or after a specified amount of computer time is used.

During the pre-clad failure period of the transient shown in Figures 1a through 1e, the cavity pressure is shown to oscillate (Figure 1c). In this study, the clad was allowed to obtain about 6% permanent deformation before failure. The large pressure drop at 1.668 seconds (times are indicated to millisecond significance only for description purposes) is caused by numerically over-straining the clad. Further studies (Figure 2b) show this problem has been mitigated.

After clad failure (Figures 1a-1e), the ejected small fuel particles cause considerable heatup, pressure increase and expansion of the interacting zone sodium. Four sodium pressure pulses and corresponding temperature increasing periods are present in Figures 1b and 1c between 1.685 and 1.705 seconds. These were caused by the 2.2 grams of initially ejected solid fuel and an additional 2 grams of molten fuel which was ejected before fuel and fission gas expulsion was temporarily reduced (between 1.698 and 1.705 seconds). The ejected fuel temperature, which can be meaningfully displayed here because of the equisized particles, rapidly drops until 1.708 seconds. This, plus the zone's expansion (lower slug reversal at about 1.692 seconds as shown in Figure 1d), heat transfer to the sodium film on the clad and to the clad cause cooldown of the interaction zone sodium. At 1.698 seconds, the decrease in sodium pressure enables commencement of significant sodium vapor generation and nearly complete voiding of the interaction zone 20 milliseconds after clad failure. During this highly voided condition, the interaction zone pressure drops below the cavity pressure allowing further fuel ejection and causing deceleration of the interaction zone interfaces. The fuel particles average temperature increases because of the lack of heat transfer and additional ejected fuel. Figure 1e shows the distribution of fuel in the coolant and its axial dependent temperature variation. About 13.5 grams of fuel are needed to fill a given coolant node, thus the maximum local volume fraction of fuel in the coolant is about 38%. In the upper portion of Figure 1e, the fuel removed from the pin is shown. The node at 111 cm shows the maximum fuel removed since this is the failed node and an additional 2.2 grams of solid fuel is removed from it.

Figures 2a through 2c show a study in which the new fragmentation model is employed. In figures 3a through 3c, fission heating of the fuel particles is not included. Inclusion of a fragmentation model causes the maximum interaction zone pressure to be reduced as indicated by the two pressure pulses obtained after clad failure. Inclusion of fission heating causes the interaction zone to become nearly void. This in turn causes a larger amount of positive sodium reactivity, increases the power, increases the fuel pin cavity pressure and thus causes more fuel to be ejected. Both runs were terminated at the hot fuel temperature condition.

Figures 4a through 4d show a single pin study in which the ejected solid fuel particles are 500 microns in diameter. At about 3.18 seconds, the fragmentation process stops because of the near saturation condition of the coolant. At this point, 2.74 grams of solid fuel at 2400°K, 0.7 grams, of fully fragmented fuel at 2400°K and 0.6 grams of partially fragmented fuel at 3750°K exists in the coolant channel interaction zone. The resultant two phase period lasts until 3.216 seconds when the vapor bubble collapses. This is caused by cooling from the upward flowing lower sodium slug at about 1100°K, heat transfer to the sodium film above and to the colder blanket region film and clad at about 1180°K. At this time, 6.1 grams of molten unfragmented fuel at an average temperature of 3725°K exists in the coolant channel in a 20 cm
axial section above and including the clad failure area. When the interaction zone sodium coolant becomes slightly subcooled, fragmentation commences again and causes the associated sodium pressure and temperature fluctuations and lower slug reversal to downward travel. The stored energy in the 6.1 grams of molten fuel is sufficient to cause the interaction zone sodium void condition. The voided zone then expands at a decreasing rate, decreasing its pressure and allowing additional fuel ejection into the voided section of the coolant channel. At about 3.28 seconds (Figure 4d), the lower interaction zone interface which is near the core midplane reverses and begins traveling upward again. The larger fragmented fuel particles, the solid particles which were not allowed to fragment and the hot portions of the clad transfer energy to the interaction zone at a combined decreasing rate. Although this run (Fortran G) was terminated after 7 minutes (IBM 360/91) it appears probable that further fragmentation events of significant masses of molten fuel would have occurred if the computer run were extended. There was about 6 grams of unfragmented molten fuel at an average temperature of 38500K located axially in a 30 cm section, above and including the clad failure location, at the termination of the run.

In the high burnup (10 at. %) study (Figures 5a-5d), as indicated in Table I (cases 2 and 8), the initial center void contains 20 times more fission gas mass than the 2 at. % burnup case. This large amount of fission gas acts as a damper and prevents large pressure increases in the interaction zone and allows larger amounts of fuel to be ejected soon after clad failure. The large voided portion of the coolant channel caused by the sodium vapor and fission gas causes a prompt critical condition.

During steady state operation, the fuel pin of Figures 6a through 6d does not have a center void or restructured fuel. Most of the fission gas is predicted to remain in the fuel and thus, as the fuel melts, relatively large amounts of fission gas will be released. Figures 6b and 6c show that a large amount of fission gas is ejected and the interaction zone temperature initially rises with little coolant pressure change. The slow temperature rise is caused by assuming large, 1000 micron radius, solid particles and the lack of large amounts of molten fuel for ejection into the coolant. The large coolant void fraction caused by the low pressure fission gas ejected at the core midplane failure node causes prompt criticality and melting of significant amounts of fuel which is then ejected.

Heatup of the interaction zone coolant causes a slight pressure increase and outward acceleration of the interfaces (lower slug reverses to downward travel at 1.978 seconds). This expansion causes some cooling. The fragmented particles also begin to transfer heat at a decreasing rate. During this period, however, large amounts of unfragmented fuel begin accumulating in the coolant channel, overcoming the cooling effects and starting another gradual heatup period of the interaction zone sodium. This heat addition causes the sodium to expand and, during the vapor formation period, the pressure buildup decreases the total void fraction as the fission gas volume decreases.

The two phase sodium condition, during which the sodium quality reaches a maximum 0.39%, ceases at 2.09 seconds as the rapid pressure increase causes a subcooled condition. At 2.09 seconds, over 100 grams of unfragmented fuel at an average temperature of 37000K exists which, according to the model, is free to fragment. In this instance, a very large interaction develops rapidly sending fuel particles to both plenums (large negative fuel feedback in Figure 6a).

After the first prompt critical period, this integrated model becomes unrealistic as the pin basically disassembles. The interaction zone model with its homogeneous description of fuel particles surrounded by flowing sodium is
no longer valid. In addition, the lack of accounting for direct molten fuel to clad heat transfer delays appreciable clad melting until 2.01 seconds. Further errors result from departure of the point kinetics assumption. This study does, however, point out the possibility of large molten fuel masses being ejected into a voided channel similar to that which might occur in a severe loss of coolant flow accident.

D. Whole Core Study

The response of the SNR-300 reactor core to a $\$.50/sec. ramp was studied. It was assumed that all the fuel pins had a peak burnup of 2 at. %. A 1.75% permanent plastic deformation clad failure criterion is used. The results shown in Figures 7a through 7e show considerable negative reactivity insertion from fuel movement associated with early high location failures of the high power fuel pins. The run was limited to 11 minutes of execution time using the IBM 360/91 computer. At termination, 6.2 MW-sec. of energy above the solid condition is stored in 10.9 kilograms (.6% of the active core fuel) of ejected molten fuel.

4. CONCLUSIONS

Within the assumptions of the newly developed model, several conclusions can be made.

1) A failure location criteria based on clad stress from the fuel cavity is sensitive to the fuel's operating power. High power pins fail higher and low power pins fail near the core midplane. This criteria is sensitive to the cavity pressure model. For higher input ramp rates ($2/sec. or greater) studies not shown here indicate high power pin failure locations nearer the midplane.

2) Fission heating of ejected fuel enhances sodium vapor formation, especially near the core midplane. In addition, it slows and can prevent (at least for larger particles) solidification in the active core region. This somewhat reduces the possibility of large quantities of fuel plating out in the core.

3) Space and time dependent fragmentation, due in part to vapor collapse, can cause oscillatory motion of ejected fuel.

4) The resultant coolant pressure pulses shown in the figures were caused by the interaction zone expanding against the inertia of large constraining slugs. Studies were made with HOPE in which the geometrical conditions of the Mark II Treat test loop were inputted. These studies indicated pressure pulses 10% to 20% of those in the figures for the same heat transfer rates to the sodium coolant.

5. REFERENCES


### Table I. Failure Time Fuel Pin Data

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1. Failure criteria based on clad reaching ultimate stress
2. Active Core: 40 cm to 130 cm, axial nodes 3 to 12
3. Failure criteria met at both nodes 9 and 10 (model chooses higher)
4. Fuel's assumed isothermal melting temperature
5. Temperature position - 1/3 thickness inward from outer surface
Figure 1. Instantaneous Fragmentation Study
Figure 2. Fission Heating Study

Figure 3. No Fission Heating Study
Figure 4. High Power, Low Burnup Pin
Figure 5. High Burnup Pin
Figure 6. Low Power Pin
Figure 7. Whole Core
Fuel expulsion from the reactor core has been widely considered as a potential neutronic shutdown mechanism following a hypothetical unprotected overpower transient accident in an LMFBR system. Previous studies, however, have been of primarily a scoping nature and, as such, were based on several simplifying assumptions regarding the dynamics of molten fuel motion.

The present study has been conducted to investigate the validity of some of these major assumptions. Consideration is given to explicit pressure and density variations of the fuel/gas mixture inside the fuel pin cavity during the expulsion process, the effects of cladding rupture size, the influence of the gas to liquid ratio of the expelled fuel, and the effects of spatial density variations and potential plateout of fuel once the expelled mixture reaches the coolant channel.

A treatment of the internal molten fuel/gas mixture as a two-phase, compressible fluid yields a markedly different reactivity feedback than that predicted with a fluid of uniform internal density and composition, but does not appear to substantially alter previous conclusions regarding the ability of a reactor to ultimately shut down safely. Cladding rupture size was found to be unimportant, whereas the composition of the fuel/gas mixture first entering the coolant channel can significantly influence the reactivity feedback behavior. Despite rather peaked density skewing distributions postulated for fuel being swept out into the coolant channel, the overall transient behavior does not appear to vary appreciably from previous studies, so long as off-center failures predominate. Early fuel plateout does not appreciably alter the reactivity shutdown mechanism, although it may become a factor regarding long-term heat removal questions.
I. Introduction

Fuel expulsion from the reactor core has been widely considered as a potential neutronic shutdown mechanism following a hypothetical unprotected overpower transient accident in a fast reactor. Although previous studies recognized the efficacy of the fuel expulsion and sweep-out process as an intrinsic shutdown mechanism, they were primarily of a scoping nature and, as such, were somewhat limited regarding many of the important details of the actual molten fuel motion process. For instance, all of the earlier studies which considered internal pin fuel motion were based on the assumption of a spatially uniform density within the molten fuel cavity, and most studies were based on a spatially uniform fuel density in the fuel-coolant interaction zone once molten fuel entered the coolant channel.

The present study has been conducted to investigate the validity of such assumptions, in addition to those concerning the void fraction of the ejected fuel-fission gas froth and the effects of molten fuel plating on the outer cladding surface. The paper is logistically divided according to the two major locations of the expulsion process: fuel dynamics inside and outside of the cladding jacket.

It should be noted that the models described in this study have been developed primarily for the time domain between initial cladding failure and the first indications of subcriticality, i.e., the time frame in which any possible autocatalytic effects should be manifested. This time domain is generally the first 100 msec or so following initial cladding rupture. Additional modeling detail will be required to fully address the questions associated with long term power-to-flow ratios and post-accident heat removal.

Although not all of the models developed have, as yet, been integrated into a consistent modeling package such as MELT-III, the influence of each of the major mechanisms investigated can be clearly noted. The results of this study, which demonstrates the wide range of accident-defining parameters which may be tolerated without approaching anything of an autocatalytic nature, provide an additional degree of confidence to the validity of the fuel sweep-out process as a viable neutronic shutdown mechanism.
The major concerns regarding the molten fuel dynamics inside the fuel pin include: (1) spatially non-uniform pressure and density; (2) cladding rupture size; and (3) composition of the fuel/gas mixture which escapes through the cladding rupture into the coolant channel.

A. Hydrodynamics of Internal Fuel/Gas Mixture

Previous analysis (8) has shown that the internal fuel density model assumed for evaluation of the consequences of a transient overpower accident can significantly affect the reactivity, particularly during the first 20-30 milliseconds following cladding failure. This result can have a substantial effect on the subsequent course of the accident. For this reason, and others to be identified below, a two-phase compressible hydrodynamics code has been developed to describe the flow of the molten fuel/gas mixture within the central cavity.

This code, identified as HOTPIM (Hydrodynamics of Two-Phase Internal Mixture), uses Hartree's form of the method of characteristics (9), modified to consider three characteristics in order to allow anisentropy, in one-dimensional, two-phase flow. The basic equations to be solved for homogeneous flow inside the central fuel pin cavity include the following:

Continuity

\[
\frac{\partial \rho}{\partial t} + \nabla \cdot (\rho \mathbf{v}) = \frac{Q}{A} - \frac{\rho}{A} \left( \frac{\partial A}{\partial t} + v \frac{\partial A}{\partial x} \right)
\]  
where \( \rho(x,t) \) = density of the molten fuel/gas froth,

\( v(x,t) \) = homogeneous flow velocity,

\( A(x,t) \) = cross-sectional area of the cavity,

and \( Q(x,t) \) = change in mass per unit length per unit time.

Motion

\[
\frac{\partial \mathbf{v}}{\partial t} + \mathbf{v} \cdot \nabla \mathbf{v} + \frac{1}{\rho} \frac{\partial \rho}{\partial x} = -\frac{Q}{\rho A} - g - \frac{\rho}{\rho A} \frac{\partial A}{\partial x}
\]  

where \( p(x,t) \) is the pressure, and \( g \) is the gravitational acceleration constant.

Conservation of Energy

\[
\frac{\partial \rho}{\partial t} + \nabla \cdot (\rho \mathbf{v}) = \frac{q - Q}{\rho A} \left( \frac{v^2}{2} \right) + p \left( \frac{\partial A}{\partial t} + v \frac{\partial A}{\partial x} \right)
\]

\[
= \frac{\rho}{A} \left( \frac{\partial \rho}{\partial t} \mathbf{v} - \frac{1}{\rho} \frac{\partial \rho}{\partial x} \right)
\]  

(3)
where \( q(x,t) \) = change in energy per unit length per unit time due to fission power,

\[ h(p,\rho) = \text{specific enthalpy of the fuel in the cavity}, \]

\[ a(\alpha, p, \rho) = \left( \frac{\partial p}{\partial p} \right)_{S}^{1/2} = \text{two-phase sonic velocity, evaluated at constant entropy } S, \]

and \( a(x,t) \) = volumetric void fraction of the mixture.

The sonic velocity for a two-phase, two-component froth\(^{10}\) in the bubbly flow regime is \(^{11}\):

\[
a(\alpha, \rho_1, \rho_2) = \left\{ \left[ \rho_2 + (1-\alpha) \rho_1 \right] \left( \frac{1-\alpha}{\rho_1 a_1^2} + \frac{\alpha}{\rho_2 a_2^2} \right) \right\}^{-1/2}
\]

(4)

where the subscripts 1 and 2 refer to properties of the pure liquid and pure gas, respectively. In characteristic form, these equations reduce to

\[
\frac{\partial p}{\partial t} + \rho a \frac{\partial v}{\partial t} = \psi_1 + \psi_2 + \psi_3
\]

(5)

along the line \( \frac{dx}{dt} = v+a \),

and

\[
\frac{\partial p}{\partial t} - a^2 \frac{\partial p}{\partial x} = \psi_3
\]

(6)

along the line \( \frac{dx}{dt} = v \), where \( \psi_1, \psi_2, \psi_3 \) represent mass, momentum, and energy changes, respectively, due to external effects. These equations are solved simultaneously on a constant Eulerian grid by using an iterative scheme to determine where the three characteristics converging on a node at time \( t + \Delta t \) intersected the time \( t \) line, and then linearly interpolating the values of \( \psi \) between nodes at \( t \) to find \( p, \rho, \) and \( v \) for a homogeneous mixture.

Relative motion between the gas and the molten fuel is treated as a perturbation to homogeneous flow. Wallis\(^{12}\) describes the steady-state drift of independent bubbles in one-dimensional vertical flow in a static liquid. The bubbles are assumed to accelerate instantaneously to their terminal velocity, and the hydrostatic pressure gradient, \(-\rho g\), is replaced by the hydrodynamic pressure gradient \( \frac{1}{A} \frac{\partial P}{\partial x} \) (pA), in Peebles and Garber's correlation for single gas bubbles in liquids\(^{13}\), resulting in a volumetric drift flux of

\[
J_{21} = 0.33 \left( \frac{V_b}{v_{\infty}} \right) \left[ -\frac{1}{\rho_1} \frac{\partial P}{\partial x} \right] \left[ \frac{0.76 \left( \frac{\rho_1}{\mu_1} \right)^{0.52}}{R_b \cdot a(1-\alpha)} \right]
\]

(7)
where $\mu$ is the viscosity,

$$R_b$$ is the bubble radius,

$$R_d$$ is the cavity radius.

where $R$ is the cavity radius.

The gas is allowed to drift according to these correlations, and a new void fraction is computed at the end of each time step, which is used for the next time step.

The assumptions used in HOTPIM are the following:

1. The flow is strictly one-dimensional.
2. The flowing medium is a two-phase, two-component fluid, with no mass transfer between the components.
3. The gas is ideal and doesn't condense, and the liquid is non-volatile.
4. The flow remains in the bubbly flow regime.
5. Flow through the cladding rupture is strictly transverse, with no velocity component parallel to the cladding wall.
6. The bubbles attain their terminal drift velocity instantaneously.
7. The correlations for steady-state drift in a hydrostatic pressure gradient apply for transient drift of fission/fill gas bubbles in molten fuel in a hydrodynamic pressure gradient.
8. The effects of dissipative forces, such as viscosity and wall friction, are insignificant.
9. There is no relative motion between pure liquid and the solid fuel grains existing when the bulk temperature of the fuel is between the solidus and the liquidus.

Although HOTPIM is designed to be included as a fuel motion subroutine in the MELT-III safety analysis program, the coupling process is not as yet complete. However, a preliminary analysis has been performed with a stand-alone version of HOTPIM for internal pin conditions approximating those found at the time of cladding failure in an irradiated fuel pin in the Fast Flux Test Facility (FFTF) during a 50$\text{\textdegree}$/sec reactivity insertion in an equilibrium cycle core. Specifically, for the 91 cm fuel length, the molten region at the time of failure was approximated by a 60 cm long cylinder with a cross section of 0.03 cm$^2$ and an initially uniform void fraction of 0.5, being heated uniformly at a rate of $2.1 \times 10^8$ ergs/sec/cm. A rupture was assumed to occur at the axial midplane with an area of 0.17 cm$^2$, when the internal pressure was 100 bars. The pressure immediately outside the rupture was assumed to remain constant at 40 bars throughout the transient.

The internal fuel density calculated by this analysis is plotted vs. axial location within the core for selected times in Fig. 1. The average density
FIGURE 1a. 1 msec AFTER FAILURE

FIGURE 1b. 3 msec AFTER FAILURE

FIGURE 1c. 7 msec AFTER FAILURE

FIGURE 1d. 30 msec AFTER FAILURE

FIGURE 1. FUEL DENSITY PROFILE INSIDE FUEL PIN
FIGURE 2. PRESSURE PROFILE INSIDE FUEL PIN

FIGURE 2a. 1 msec AFTER FAILURE

FIGURE 2b. 3 msec AFTER FAILURE

FIGURE 2c. 7 msec AFTER FAILURE

FIGURE 2d. 30 msec AFTER FAILURE
in the cavity is also shown, for comparison with a uniform density model. It can be clearly seen from these figures that the compressibility of the internal froth results in taking fuel preferentially out of the regions of the pin closest to the rupture location. This results in a considerably larger reactivity decrease, relative to the uniform density case, if the failure location is near the axial midplane because more fuel is removed from the high-worth region of the core. Should the failure location be near the pin extremity, the converse is true, but in this case there is little concern for precise detail because the overall result of a failure location far away from the axial midplane is very rapid neutronic shutdown.

The total amount of fuel expelled from the molten cavity appears to be less, using HOTPIM, than would be the case for the uniform density assumption. Although this should somewhat delay the shutdown process, from a reactivity loss standpoint, it should also smooth out the flow process and allow more time for fuel sweepout to occur once the fuel mixture enters the coolant channel. This difference in behavior, between the standard uniform density model and the more realistic hydrodynamics model, is that in the former case pressure equilibrium is established instantaneously in the entire cavity. For the numerical example presented here, such an assumption would result in an immediate uniform density decrease to 2.46 g/cm³, followed by a further gradual decrease due to additional fuel melting and expulsion. The time-dependent internal pin pressures predicted by HOTPIM are shown in Figure 2.

B. Cladding Rupture Size

Since cladding rupture characteristics are not well known, the effects of cladding rupture size on fuel sweepout have been investigated by varying the effective orifice area over several orders of magnitude. Figure 3 contains MELT-III results of fuel motion reactivity feedback, for a uniform motion case during a 500/sec overpower transient, for effective rupture areas of 0.017 cm² to 1.7 cm². The lower extreme represents a classic pin hole and the larger extreme corresponds to a rupture area approximately five times that of the cross-sectional area of the fuel pin. The negative reactivity feedback due to fuel sweepout is noted to have essentially identical time-dependent properties over a wide range of rupture sizes. For very small ruptures, the feedback effect is delayed due to higher flow resistances characteristics of small orifices. However, the same curve of fuel reactivity vs. time applies to within a few percent over the first fifty milliseconds after failure for any rupture with characteristic dimensions greater than about three millimeters.

C. Fuel/Gas Composition

The reactivity feedback is strongly dependent on the void fraction of the fission gas/molten fuel mixture escaping the fuel pin. The parameter GTOL (gas to liquid ratio), defined as the volume fraction of gas passing through the rupture as long as enough fuel and gas are present, was introduced into MELT-III in order to analyze the fuel motion reactivity for a range of void fractions. GTOL can be computationally varied from 0 to 1, where the lower value maximizes the fission gas content. As might be expected, the negative feedback is enhanced by increasing fuel content in the mixture, since more fuel is actually being swept out of the high-worth.
FIGURE 3. INFLUENCE OF CLADDING RUPTURE AREA ON FUEL SQUIRTING DYNAMICS
FIGURE 4. INFLUENCE OF FUEL/GAS COMPOSITION ON THE REACTIVITY CONSEQUENCES OF FUEL EJECTION.
regions of the core. It is seen from Figure 4 that the reactivity feedback is very strongly dependent on the parameter GTOL, increasing by a factor of five or more after 35 milliseconds as the molten fuel fraction increases from one-third to one. The dotted curves in the figure correspond to homogeneous flow.

III. Outside Cladding Jacket

Factors influencing the reactivity feedback of fuel motion after the expelled fuel/gas mixture reaches the coolant channel include: (1) the axial density gradient of the fuel/gas mixture in motion, and (2) the degree of fuel plateout which might occur due to freezing or mechanical packing in the coolant channel. Should the latter effect occur to any appreciable extent, subsequent flow patterns through the core are also affected.

A. Axial Density Gradients

Whereas recent work has explored the sensitivity of the fuel sweepout shutdown mechanism to important parameters such as axial failure location and magnitude of the local fuel/coolant interaction, most studies have been based on a homogeneous FCI zone containing a uniform fuel density. This assumption, although sufficient while the FCI zone is small, is questionable when fuel expulsion occurs later during the interaction zone expansion. A recent attempt to address the problem mechanistically was based on a one-dimensional Lagrangian model superimposed on an Eulerian mesh. However, because of uncertainties associated with fuel/sodium vapor slip during the sweepout process and the possibility of some molten fuel freezing on coolant channel structures, the present approach has been to bound the potential implications of a non-uniform fuel density via a parametric process. This study, performed with a two-channel representation of the FFTF core using the MELT-III code, simulates non-uniform density by calculating the reactivity feedback from expelled fuel in the coolant channel region according to an assumed density skew within the FCI zone. The skew can be peaked at any desired axial location and the degree of skew can be varied parametrically.

Figure 5 shows both the fuel density and the associated reactivity for each node in the FCI zone for three assumed degrees of skew, at 20 milliseconds after fuel pin failure. The simulated density skews are based on the expression \( Y^n \), where \( Y \) is a user input quantity and \( n \) is the number of integral nodes distant from the rupture location at which the density is being evaluated. The skews shown in Figure 5 are \( 1.0^n \) (the uniform density case), \( 0.7^n \), and \( 0.5^n \). The density profile is realistically centered on the rupture location.

The fuel density in the coolant channel assuming a skew of \( 0.7^n \) is shown in Figure 6 at 4, 7, 20, and 50 msec after fuel pin rupture, wherein the expansion of the FCI zone can be seen (the nodal values are here connected with smooth lines for clarity). An interesting effect is observed in that the fuel density drops initially after failure (7 msec) but then increases again. This effect is the result of a fairly rapid drop in the fuel pin internal pressure which reduces the pressure differential between pin and coolant channel. The FCI zone expansion spreads out the initially-expelled fuels and decreases the average density. Eventually the FCI zone tends to overexpand and simultaneously reaches a cooler structural zone, where sodium vapor condensation becomes more pronounced. This causes an increase in the expulsion rate, which again raises the average fuel density.
FIGURE 5a. FUEL DENSITY DISTRIBUTION IN COOLANT CHANNEL FOR VARYING SKEW

FIGURE 5b. REACTIVITY DISTRIBUTION OF FUEL IN COOLANT CHANNEL FOR VARYING SKEW
FIGURE 6. AXIAL FUEL DISTRIBUTION IN COOLANT CHANNEL AT SELECTED INTERVALS AFTER FAILURE BUT PRIOR TO FULL SWEEPOUT. NO PLATING.
FIGURE 7a. Reactivity feedback from fuel motion for varying fuel density skew in coolant channel. No plating.

FIGURE 7b. Reactivity feedback from fuel motion for varying cladding failure locations.
density in the coolant channel. The reactivity balance given in Figure 6 shows the amount of reactivity lost from inside the fuel pin and correspondingly the amount of reactivity gained as the fuel is placed in the coolant channel. The net reactivity tends to be a relatively small difference between two large terms. This observation provided, in part, the motivation to better scope the effect of skewed fuel distributions both inside and outside of the fuel pin.

A more conservative study, not presented here, has been performed where the assumed density profile is centered on the axial midplane of the core. This study was equivalent to a large slip between sodium vapor and the fuel fragments leading to a pile-up at the lower sodium interface. In it the integrated reactivity feedback from fuel motion was larger for a given rupture location, but the accident progression path was essentially the same.

Figure 7a presents the reactivity feedback from fuel motion versus time for several degrees of skew. Figure 7b is the reactivity feedback from fuel motion versus time for different axial failure locations assuming a skew of 0.7 in. Simulated plating, which is discussed in the next section, has been incorporated into the analyses shown in Figure 7b, but the shape of the reactivity curves is only slightly different for a no plating case up to the first 100 milliseconds. The major effect of the assumed density skew for non-midplane failure locations is to delay the negative reactivity feedback from fuel motion as the FCI zone expands upward out of the neutron flux of the core region. Eventually the same amount of negative reactivity is realized for the case of no plating, when the lower interface of the FCI zone effectively sweeps out all of the fuel in front of it. It should, perhaps, be pointed out that the model reached a geometrical constraint before the case of midplane failure could be completed in that the interaction region left the instrument tree flow guide tube and entered the upper sodium pool. However, the lower coolant slug was traveling upward again at this time, and would in time sweep out all the fuel which had not plated. Hence, the core would eventually reach a subcritical state less negative than the other failure locations.

B. Simulation of Expelled Fuel Remaining Behind in the Core Coolant Channels

In order to assess the reactivity consequences of expelled fuel remaining behind in the coolant channel instead of cleanly sweeping out with the returning sodium coolant, a preliminary fuel plateout model was incorporated into MELT-III. The plating model simulates fuel being lodged in the coolant channel either due to molten fuel freezing on structure or being mechanically lodged in the helical flow channels. The amount plated in each node during each time step is proportional to the density of the expelled fuel in the FCI zone, as specified by the skewing model. Therefore, the plating is also peaked sharply near the fuel pin rupture location. The plating model further provides for heat generation by the fuel that is plated-out, as well as hydraulic resistance to sodium flow as a result of the decreased flow area.

Figure 8a indicates, for one sample calculation, the total amount of fuel being plated as well as the total expelled. This case represents a plating of 1% of the amount of molten fuel in each axial node during each millisecond time step. Figure 8b contains a representative axial profile of plated-out fuel for 10 and 100 milliseconds after fuel pin failure. This
FIGURE 8a. FUEL EXPULSION AND PLATING VS. TIME

FIGURE 8b. AXIAL FUEL PLATE-OUT DISTRIBUTION
case represents an assumed skew of 0.7° and plating rate of 1% per msec.
The amount of fuel plated after 100 msec is equivalent to approximately a 30% reduction in flow area at the rupture point.

IV. Conclusions

This study was performed in order to determine the sensitivity of various fuel motion parameters on the fuel expulsion and sweepout feedback mechanism. Although experimental justification for selecting a particular range of values for many of the parameters is limited, reasonable physical constraints, combined with the parametric analyses conducted in this study, can be often used to greatly reduce the range of concern.

First, the assumption of a uniform material density of the fuel/fission gas mixture inside the central molten cavity of the fuel pin is known to be physically incorrect—it was simply used in the past for computational ease. Although the HOTPIM model is not yet tied in to the overall MELT-III computational package, the preliminary results from this hydrodynamics model would directly imply an enhanced shutdown mechanism relative to the uniform density model, if the axial failure location should be near the core midplane. This is the only region of primary concern, since any failure location significantly off center results in a very strong negative feedback mechanism, and a substantial reduction in the rate of fuel loss would be required in order to have any appreciable detrimental effect on the shutdown process.

The actual size of the cladding rupture for any given transient is not well known, but from the parametric results of this study, a knowledge of this size appears relatively unimportant. A far more important parameter is the gas to liquid ratio in the fuel/gas mixture which flows from the molten pin cavity into the coolant stream. Initial results using the HOTPIM within MELT-III in order to provide a set of consistent pressure boundary conditions.

A uniform fuel density in the fuel-coolant interaction zone and a full sweepout of fuel fragments was assumed in earlier work as an expediency to allow scoping of the implications from molten fuel motion. The fuel coolant interaction zone will undoubtedly have a fuel density gradient within it, but it is difficult to envision how such a gradient could exceed the severe skews investigated in this study. It should be noted, by way of perspective, that the axial failure location still remains the dominant parameter in determining the course of the accident, and the models explored herein do not appear to substantially alter the accident progression path for off-midplane failure locations. The only instances in which anything of an autocataclysmic nature began to appear, even with the highly skewed cases including fuel plateout, were those where massive midplane failure was postulated—a circumstance which is in contradiction with existing experimental results. Some fuel plateout or partial plugging would be expected to occur during the sweepout process, but any such plateout does not appear to sensitively affect the reactivity feedback mechanism during the early shutdown process. The primary effect of plating is to determine the final margin of subcriticality and the long range coolability of the failed subassemblies.
References

1. W. E. Kastenberg and E. T. Rumble, "Preliminary Analysis of the Pre-


5. P. G. Lorenzini and G. F. Flanagan, "An Evaluation of Fuel-Coolant Inter-


12. Ibid., pp. 247-55.


The unprotected overpower accident in an LMFBR is one of the hypothetical core disruptive accidents whose analysis is usually considered in the design and licensing of the reactor. Earlier analyses of this accident performed for the SNR-300 have been overly pessimistic because the reactivity feedback due to fuel movement during molten fuel-sodium interaction had been neglected in the predisassembly phase. Kastenberg and Rumble have developed a simple model to calculate fuel motion and its reactivity effect during this phase of the accident and have shown its potential for lowering the work energy for the SNR-300. However, in their model the failure conditions and the ejection of molten fuel into the coolant stream could only be postulated.

Two recently developed modules of the SAS3A computer program permit a more detailed analysis of this accident. Failure conditions such as time and position of clad failure can be determined from the fuel and clad mechanical interaction predicted by the DEFORM-II module. After clad failure the SAS/FCI module predicts the ejection of molten fuel and fission gas into the sodium coolant, the thermal interaction between fuel and sodium, and the reactivity feedback from the fuel motion and the sodium voiding. In addition, fuel restructuring and burnup effects, such as fission-gas retention and swelling on which the failure conditions and fuel ejection strongly depend, can be determined internally by a steady-state fuel characterization routine. The objective of this paper is to present some results for an unprotected $\frac{1}{sec}$ overpower accident in the SNR-300 calculated with this improved computer program. The calculations were done for a 100-day core and for an equilibrium core at the beginning of the 117-day fuel cycle. Clad failure was predicted by three different failure criteria.

1.0 Parameters Used in the Calculations

The SNR Mark-1 core cross section is shown in Fig. 1. The Mark-1 core is a two-region core with six subassembly rings in the inner region and two rings in the outer region. The SAS3A ten-channel representations for this core are given in Table 1. For the 100-day core each subassembly ring was represented by one channel except for Ring 6, which was modeled by two channels to distinguish subassemblies from the two enrichment zones. For the equilibrium core each SAS3A channel represented subassemblies of similar burnup and power level.
Because the maximum number of SAS3A channels was limited to ten, subassemblies from different core rings had to be lumped together.

Power, reactivity, and thermohydraulic data for the Mark-1 core were taken from Ref. 1. For these calculations the control rods between the inner and outer core regions were assumed to be inserted halfway into the active core. Thus, although the peak power was near the midplane in the inner core region, it was shifted down to 36% of the active height in the outer region. The assumed steady-state peak linear power of 14.6 kW/ft was considerably greater than the corresponding value for the FFTF, but more importantly, the maximum positive sodium-void worth was 3.5 dollars, so that for the SNR large reactivity increases were achieved from voiding of the central core region.

Since the fission-gas plenum of the SNR is positioned below the core, the resistance to flow upward from the core was much less than downward from the core. Consequently, sodium above the core was quickly accelerated during an FCI, and sodium voiding and fuel sweepout proceeded faster upward than downward.

The parameters for simulation of restructuring and burnup and the basic parameters of SAS/FCI used in the calculations are given in Table 2. Only two fuel zones (columnar and unstructured) were defined. Fission-gas retention in the unstructured fuel zone was calculated from Dutt's correlation. The fuel porosity from fission-gas swelling was assumed to increase linearly at 1.5% AV/V per a/o burnup.

The SAS/FCI parameters were selected following a sensitivity study in which the influence of the model parameters was investigated. The sensitivity of some FCI results to parametric changes are summarized in Table 3. Although the study was performed for FFTF high-power irradiated fuel pins, the effects would be similar for SNR-type fuel elements. In the SNR calculations the cavity pressure, volume, and composition at failure were determined by the program. They depended strongly on the failure criterion, fuel restructuring, and the amount of retained fission gas which was assumed to be released as fuel melted in the cavity. Pin failure was assumed to result in a rip 5 cm long and 1 cm² area. For high failure pressures (over 500 atm) these values were increased to 15 cm and 3 cm² respectively. Recent comparisons of SAS/FCI with the more detailed PLUTO model indicate that the agreement between the two models is better when the cavity cross-sectional area is used as the fuel flow area in the modeling of the time-dependent ejection process. At the time these calculations were done, the results of this comparison were not available and therefore the parameters suggested in this comparison were not used in these studies.

The parameters for the FCI zone were identical to the base-case parameters in the first column of Table 3. Instantaneous fragmentation into particles of 100 μ radius was assumed for the ejected fuel. After slug expulsion a 0.015-cm liquid-sodium film was left behind on the clad and structure surfaces. Heat transfer to the colder portions of clad and structure was included.

2.0 Results of the Accident Calculations

Characteristic results of the accident calculations are summarized in Table 3. Prompt criticality and core disassembly were only predicted for the 100-day core calculation.

2.1 Results for the 100-day Core

Pin failure for the 100-day-core ramp accident was assumed to occur at a 30% fuel-mass melt fraction at the peak power axial position. The axial failure position in each channel was also assumed to be near the peak power node.
This failure criterion resulted in calculated pin-failure pressures of approximately 100 atm. The fuel-coolant interaction following pin failure was energetic with a peak two-phase pressure near 80 atm.

The calculated transient reactor power and net reactivity are shown in Fig. 2. Due to the programmed reactivity insertion, the power increased and the resulting fuel-temperature increase caused a strong negative Doppler reactivity feedback whose rate soon almost compensated for the programmed reactivity insertion rate. The central assembly was predicted to fail first at 1.28 sec after the accident initiation. The subassemblies of Ring 2 failed about 15 msec later. Coherent voiding of the six subassemblies of Ring 2 created a large positive sodium-void reactivity insertion rate of $22/\text{sec}$. The reactivity was not compensated by fuel sweepout and the reactor became prompt critical at 1.31 sec as shown by the rapid power increase in Fig. 2. This power increase caused the high-power subassemblies of Ring 6 (represented by SAS3A Channel 7) and the subassemblies of Rings 3 and 4 to fail within 2 msec. In addition, the rapid melting rate achieved after prompt criticality caused a strong fuel ejection into the midplane of the core. This fuel-motion reactivity provided a large positive reactivity insertion ($\gtrsim 100/\text{sec}$) such that temperatures sufficient to vaporize the fuel were soon attained. At this point the SAS/FCI model was no longer applicable and the calculation was terminated. To carry the analysis of this accident to its termination would require further analysis using a disassembly program such as VENUS-II. Such an analysis was not done.

2.2 Results for the Equilibrium Core

For the core at the beginning of the equilibrium cycle, two different failure criteria were applied to investigate two different modes of clad failure.

1. For the rupture-strain failure criterion, pin failure was assumed to result from differential thermal expansion between the fuel and clad. Time and position of clad failure were determined from a separate deformation analysis using the SAS2A/DEFORM-II code.

2. For the burst-pressure failure criterion, pin failure was assumed to result from high pressure in the fuel-pin cavity. The pressure source was the released fission gas in the molten cavity of the fuel pin.

Both simulations predicted reactivity decreases through fuel expulsion prior to reaching prompt criticality. Fuel elements in a given core ring no longer failed coherently because each burnup level for a given ring was modeled by a different channel. In addition, fresh fuel pins were predicted to remain intact for both calculations.

2.2.1 Accident Sequence with Rupture-strain Failure Criterion

The rupture-strain criterion predicted early failure for irradiated pins. In the DEFORM-II analysis, clad failure for the 117-day pins was assumed at the time and axial position where the clad temperature and equivalent plastic strain exceeded estimated values of 650°C and 0.5% plastic deformation. For the 234-day pins, clad failure was predicted only by a clad-temperature criterion of 650°C. Although no fluence- and temperature-dependent rupture-strain data presently exist for the 498SS clad used in the SNR, the applied rupture strains reflect the high-temperature embrittlement of irradiated cold-worked stainless steel.

Using the above criteria, the pins with 234 days irradiation were predicted to fail before those of 117 days irradiation. The fuel elements
of 234 days irradiation were predicted to fail at a relative height of 75% with a peak node melt fraction of 5%. Those with 117 days irradiation were predicted to fail slightly above the midplane when 10% fuel melt fraction was achieved. Cavity pressure at failure differed very little from the steady-state central void pressures. Thus, only small amounts of fuel were ejected into the coolant channel and the resulting FCI was rather weak with peak two-phase sodium pressures near 10 atm. For the 234-day pins, this pressure was insufficient to reverse the flow of the sodium and the inner core region did not void.

Figure 3 shows the calculated time dependence of the net reactivity and normalized power with the failure times for the channels marked. The highly irradiated fuel of Ring 2 (SAS3A Channel 3) was predicted to fail first at 1.1 sec. Failure of the high-burnup pins resulted only in negative reactivity feedback both from fuel expulsion and from the negative sodium-void reactivity worths at the high axial failure location. Only a small positive void reactivity feedback at a rate of $2/sec resulted from failure of the medium-burnup pins in the central core region. No large amount of molten fuel accumulated in the cavity after the early failures and the reactivity gradually decreased as molten fuel was ejected into the coolant channel and swept out of the core.

2.2.2 Accident Sequence with Burst-pressure Failure Criterion

A completely different accident sequence resulted when the pin failure was determined from the burst-pressure failure criterion. This criterion was based on the assumption that the cavity pressure loaded the clad through the loading of essentially strengthless fuel. To obtain the pressure on the inside of the clad, the cavity pressure was reduced by the ratio of the fuel melt radius and the inner clad radius. Clad hoop stresses were calculated from this pressure loading by the thin-wall approximation and compared with the ultimate tensile strength of the clad material. Thus, the ultimate tensile strength became the key material property for determining clad rupture. For these calculations it was fit to high strain-rate rupture stresses for unirradiated 20%-CW 316SS.\(^{11}\)

With this criterion all channels were predicted to fail slightly above the axial midplane. Cladding failure temperature was around 830°C. Failure pressures in the cavity were between 700 and 800 atm and peak node melt fractions were near 45%. The higher failure pressures and greater melt fractions together with the greater failure rip area and length used in this calculation caused more fuel to be ejected into the coolant channel after failure. Fuel mass ejected into single-phase sodium was almost three times higher than obtained with the 30%-melt-fraction criterion. The resulting FCI became energetic with two-phase sodium pressures greater than 100 atm. Core voiding proceeded rapidly in both directions, creating void reactivity rates of up to $25/sec. But it also resulted in a strong negative feedback from fuel expulsion 7 msec after failure.

Sodium-void and fuel-motion feedback from Channel 2, which was the first channel to fail in this simulation, are plotted in Fig. 4 as functions of time after failure. One can see the delay between the initially strong positive reactivity insertion due to core voiding and the strong negative reactivity due to fuel expulsion. The sodium-void reactivity soon approached an asymptotic value after the core region became voided, but the fuel-motion reactivity always strongly decreased as more and more fuel was ejected into the coolant channel and swept out of the core.

Calculated transient power and net reactivity for this accident are shown in Fig. 5 together with the total sodium-void and fuel-motion feedback.
The medium-burnup fuel of Channel 2 was predicted to fail first 1.39 sec after the accident initiation. Sodium-void and the net reactivity increased rapidly due to the FCI and caused the first strong power rise after failure. Fuel sweepout became effective before the medium-burnup pins of Channel 5 failed and thus the power and net reactivity were soon reduced somewhat. But the FCI which resulted from the failure in Channels 5 and 3 caused further increases in reactivity and power. The net reactivity achieved a maximum ramp rate of $17/sec$, and peak value of 29 and 96$^o$ were calculated for the normalized power and net reactivity respectively 18 msec after the first pin failure. Then the strong negative fuel-motion feedback from all failed channels caused a rapid shutdown of the reactor before the net reactivity would have reached prompt critical.

The calculated strong negative fuel-motion feedback was overestimated in the later stages of the FCI. The rather short upper liquid slugs gained such extremely high velocities after they left the active core region that slip between fuel and sodium could no longer be neglected. Prompt criticality might have been reached as for the 100-day core if fuel-motion reactivity had been calculated with some allowance for slip flow.

Medium-burnup pins were predicted to fail prior to the high-burnup pins in this simulation due to the assumed linear porosity increase with volume. For these calculations the available porosity volume increased more with burnup than did the amount of retained fission gas. Thus, during the transient, fission-gas release and fuel melting built up less cavity pressure in the high-burnup pins and the burst-pressure criterion was met at a later time. A different relation for calculating the fuel porosity, such as coupling it to the amount of retained fission gas, could lead to a different failure and accident sequence.

All coolant channels in this simulation were hydraulically coupled by the SAS3A primary-coolant-loop model. Figure 6 shows the resulting variations of the inlet pressure as a function of the transient time. The calculated coolant inlet pressures did not increase as much as the FCI zone pressures. A maximum inlet pressure of 24 atm was reached shortly after failure of Channel 3. The higher inlet pressure slightly reduced the lower liquid slug acceleration and increased the upper liquid slug acceleration, thereby causing slightly greater fuel sweepout in the late-failing channels. However, the effect was small. The increased inlet pressure can change the failure sequence since it causes higher coolant mass flow rates (up to 30% greater than steady state values) in the intact channels. Thus clad failure can be delayed due to the increased cooling.

3.0 Conclusions

For the three calculations presented here the final state of the SNR reactor was predicted to be different for each calculation. The most serious sequence was calculated for the 100-day core in which the assumed failure location was near the peak power node for all channels. When the inner core regions were coherently voided, fuel sweepout, which was the major shutdown mechanism, could not become effective before prompt criticality was reached. In fact, after prompt criticality was achieved, further pin failures and fuel ejection into the core midplane caused additional positive reactivity effects which eventually led to disassembly conditions for the reactor.

On the other hand, calculations for the rupture-strain criterion resulted in a mild FCI with negative fuel-motion reactivity and even some negative sodium-void reactivity. Although the net reactivity gradually decreased as molten fuel was swept out of the core, a complete termination of the accident in the sense
of a stable, subcritical, coolable reactor geometry was not achieved in the calculation. As Fig. 3 shows, at the termination of the calculation the net reactivity was again increasing due to the programmed driving reactivity. The reactor could not be guaranteed to remain subcritical unless the driving reactivity were limited to a few dollars or a large amount of fuel were swept out of the core. Even then, enough negative reactivity would eventually have to be inserted to assure shutdown to decay power levels.

The burst-pressure criterion resulted in a very energetic FCI with rapid core voiding and high rates of reactivity insertion, but it also caused a rapid shutdown of the reactor before prompt criticality was attained. From the calculation it appears that the reactor would achieve sufficient subcriticality to permit postaccident cooling if the driving reactivity were limited. However, the mechanism of clean sweepout without plateout or plugging has not been verified by experiment, and a plugging-type behavior could lead to difficulty in demonstrating that postshutdown cooling could be established. The assumed rapid heat transfer between fuel particles and sodium may also tend to overpredict the amount of fuel sweepout. In addition, if fuel sweepout were calculated more realistically with respect to slip between fuel particles and fission gas or sodium vapor, this calculation may have led to core disassembly as for the 100-day core. For an end-of-equilibrium-cycle core in which all fuel pins would retain considerable fission gas, the burst-pressure criterion also might have led to as severe an accident as the 100-day core calculation.

These studies have also shown the extreme sensitivity of accident results to the assumed clad-failure criterion in a transient overpower accident calculation. Both the burst-pressure criterion and the rupture-strain criterion predicted the failure time reasonably well for the TREAT H5 experiment. However, further experimental information is needed to adequately verify the mode and position of clad failure so that failure uncertainties in calculations such as these can be decreased. Some TOP experiments in the European CABRI reactor are being designed specifically to study the failure mechanism in irradiated fuel. Also, further fuel-failure-threshold experiments are being performed by both ANL and HEDL in the TREAT reactor. Such experimental investigations can give additional knowledge regarding failure locations, failure mode, and failure pressures in both fresh and irradiated fuel pins.
References


### Table 1
SAS Channel Setup for SNR Ramp Accident Analyses

<table>
<thead>
<tr>
<th>Subassemblies</th>
<th>SAS Channel</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>2</td>
</tr>
<tr>
<td>Ring</td>
<td>1</td>
</tr>
<tr>
<td>Peak linear Power [kW/ft]</td>
<td>14.6</td>
</tr>
</tbody>
</table>

### Table 2
Important Parameters for the SNR Ramp Simulations

**Restructuring + Burnup**

- Columnar grain region isothermal: $1720 \, ^\circ C$
- Fission gas retention: Dutt's relation ref. $^67$
- SS-fuel swelling $\frac{\Delta V}{V}$: 1.5 %/at % burnup

**PCI-Parameters**

- Squirt option: Molten fuel and fission gas are squirted proportional to their respective volume fractions in the cavity
- Failure rip length: 5 cm (15 cm)$^1$
- Failure rip area: $1 \, cm^2$ (3 cm$^2$)$^1$
- Inertial length: 10 cm
- Fuel particle radius: 100 μ (instantaneous fragmentation)
- Sodium film thickness: 0.015 cm
- Condensation heat transfer coefficient: 6.3 W/cm$^2$ °C

$^1$ These values were used for high failure pressures > 500 atm (burst pressure criterion)
Table 3  Important Parameters of the SAS/FCI Model and Their Influence on Different FCI Results

<table>
<thead>
<tr>
<th><strong>Pin Failure Pressure</strong></th>
<th>atm</th>
<th>100</th>
<th>50</th>
<th>200</th>
<th>Critical flow conditions are met only during the first 0.5 msec and barely influence the rate of ejection. Higher failure pressures cause higher initial ejection rates and earlier sodium vaporization which limits the amount of fuel ejected into single phase sodium.</th>
</tr>
</thead>
<tbody>
<tr>
<td>start of sodium vaporization</td>
<td>m/sec</td>
<td>4.6</td>
<td>6.1</td>
<td>3.3</td>
<td>Higher failure pressures cause higher initial ejection rates and earlier sodium vaporization which limits the amount of fuel ejected into single phase sodium.</td>
</tr>
<tr>
<td>molten fuel ejected into single phase sodium</td>
<td>g</td>
<td>4.8</td>
<td>4.0</td>
<td>5.2</td>
<td>Reactivity effects from sodium voiding and fuel ejection. For high failure position voiding of inner core region only through motion of lower liquid slug. Earlier fuel ejection from top of core. Different inertias of liquid slugs. FCI zone expansion more restrained when upper and lower slugs have the same lengths. Liquid failure position. Liquid sodium film in FCI zone smaller at lower failure positions. Larger ejection period into single phase sodium.</td>
</tr>
<tr>
<td>max. 2-phase sodium pressure</td>
<td>atm</td>
<td>75</td>
<td>67</td>
<td>80</td>
<td>Fuel ejected into single phase sodium. Smaller condensation heat transfer. The accumulated fuel enthalpy becomes smaller though more fuel is ejected into single phase sodium because of later start of sodium boiling. Peak 2 phase pressures become smaller.</td>
</tr>
</tbody>
</table>

**Fresh Gas Release upon Fuel Melting**

<table>
<thead>
<tr>
<th>g/g - m,</th>
<th>0.00055</th>
<th>0.00533</th>
<th>0.00322</th>
<th>The gas serves as pressure reservoir which helps the cavity pressure. It also makes the FCI zone more compressible. A higher gas release allows higher ejection velocities and more fuel to be ejected into single phase sodium.</th>
</tr>
</thead>
<tbody>
<tr>
<td>max. ejection velocity</td>
<td>cm/sec</td>
<td>320</td>
<td>290</td>
<td>210</td>
</tr>
<tr>
<td>molten fuel ejected into single phase sodium</td>
<td>g</td>
<td>4.8</td>
<td>4.0</td>
<td>5.2</td>
</tr>
<tr>
<td>max. 2-phase sodium pressure</td>
<td>atm</td>
<td>75</td>
<td>80</td>
<td>101</td>
</tr>
</tbody>
</table>

**Composition of the Ejected Materials**

| - | molten fuel | molten+solid fuel | gas | Including also the solid fuel in the mixture causes more fuel to be ejected, but with a smaller specific enthalpy. Ejection velocities decrease because of higher mixture density. Without the froth and central void gas creates an additional pressure reservoir in the cavity. Lack of gas in FCI zone reduces ejection rate into single phase sodium because of poorer compressibility. |

**Failure Position**

<table>
<thead>
<tr>
<th>0/o active height</th>
<th>75</th>
<th>50</th>
<th>25</th>
<th>Reactivity effects from sodium voiding and fuel ejection. For high failure position voiding of inner core region only through motion of lower liquid slug. Earlier fuel ejection from top of core. Different inertias of liquid slugs. FCI zone expansion more restrained when upper and lower slugs have the same lengths. Liquid failure position. Liquid sodium film in FCI zone smaller at lower failure positions. Larger ejection period into single phase sodium.</th>
</tr>
</thead>
<tbody>
<tr>
<td>fuel ejected into single phase sodium</td>
<td>g</td>
<td>4.8</td>
<td>5.5</td>
<td>6.8</td>
</tr>
<tr>
<td>Start of sodium vaporization</td>
<td>m/sec</td>
<td>4.6</td>
<td>5.0</td>
<td>6.5</td>
</tr>
<tr>
<td>Fuel sweep out from top of core</td>
<td>m/sec</td>
<td>13</td>
<td>22</td>
<td>37</td>
</tr>
<tr>
<td>max. net reactivity feedback</td>
<td>Cents</td>
<td>0</td>
<td>19</td>
<td>12</td>
</tr>
<tr>
<td>Time of max. pos. feedback</td>
<td>m/sec</td>
<td>0</td>
<td>7</td>
<td>29</td>
</tr>
</tbody>
</table>

**Failure Rip Length**

<table>
<thead>
<tr>
<th>cm</th>
<th>5</th>
<th>1</th>
<th>10</th>
<th>More sodium is in initial FCI zone which increases rip length. Fuel to sodium mass ratio becomes smaller though more fuel is ejected into single phase sodium because of later start of sodium boiling. Peak 2 phase pressures become smaller.</th>
</tr>
</thead>
<tbody>
<tr>
<td>fuel to sodium mass ratio at onset of boiling</td>
<td>m/sec</td>
<td>4.6</td>
<td>3.5</td>
<td>4.9</td>
</tr>
<tr>
<td>max. 2-phase pressure</td>
<td>atm</td>
<td>75</td>
<td>10</td>
<td>60</td>
</tr>
</tbody>
</table>

**Fragmentation Heat Transfer**

<table>
<thead>
<tr>
<th>m/sec</th>
<th>0</th>
<th>5</th>
<th>10</th>
<th>More fuel is ejected into single phase sodium when the fuel cannot transfer heat immediately after entering the FCI zone. The ejection process does not stop before fragmentation and onset of heat transfer. The accumulated fuel enthalpy causes rapid pressure rise after fragmentation.</th>
</tr>
</thead>
<tbody>
<tr>
<td>fuel to sodium mass ratio at onset of boiling</td>
<td>m/sec</td>
<td>4.6</td>
<td>6.0</td>
<td>11.0</td>
</tr>
<tr>
<td>max. 2-phase pressure</td>
<td>atm</td>
<td>75</td>
<td>128</td>
<td>169</td>
</tr>
</tbody>
</table>

**Sodium Film Thickness**

<table>
<thead>
<tr>
<th>cm</th>
<th>0.015</th>
<th>0.0</th>
<th>0.030</th>
<th>The liquid sodium film assumed to be left behind from the expelled slugs increases the sodium mass in the FCI zone. It reduces the sodium saturation pressure, decreases the void fraction and has a direct influence on the void reactivity feedback.</th>
</tr>
</thead>
<tbody>
<tr>
<td>max. 2-phase sodium pressure</td>
<td>atm</td>
<td>139</td>
<td>284</td>
<td>76</td>
</tr>
<tr>
<td>FCI zone sodium mass at 20 msec in units of initial sodium mass</td>
<td>7.4</td>
<td>1</td>
<td>11.3</td>
<td>The liquid sodium film assumed to be left behind from the expelled slugs increases the sodium mass in the FCI zone. It reduces the sodium saturation pressure, decreases the void fraction and has a direct influence on the void reactivity feedback.</td>
</tr>
<tr>
<td>void fraction at 20 msec</td>
<td>0/o</td>
<td>74</td>
<td>88</td>
<td>53</td>
</tr>
</tbody>
</table>

**Condensation Heat Transfer**

<table>
<thead>
<tr>
<th>W/cm²°C</th>
<th>6.3</th>
<th>6.0</th>
<th>0.43</th>
<th>0.0</th>
<th>The sodium temperatures is ca. 900°C above the cladding temperature at onset of boiling. Heat transfer to clad and structure is a major source of pressure relief in the FCI zone. Smaller condensation coefficients increase the FCI zone pressure and delay further ejection of fuel into 2 phase sodium.</th>
</tr>
</thead>
<tbody>
<tr>
<td>max. 2-phase pressure</td>
<td>atm</td>
<td>75</td>
<td>100</td>
<td>102</td>
<td>The sodium temperatures is ca. 900°C above the cladding temperature at onset of boiling. Heat transfer to clad and structure is a major source of pressure relief in the FCI zone. Smaller condensation coefficients increase the FCI zone pressure and delay further ejection of fuel into 2 phase sodium.</td>
</tr>
<tr>
<td>max. sodium temperature</td>
<td>°C</td>
<td>1700</td>
<td>1800</td>
<td>1820</td>
<td>The sodium temperatures is ca. 900°C above the cladding temperature at onset of boiling. Heat transfer to clad and structure is a major source of pressure relief in the FCI zone. Smaller condensation coefficients increase the FCI zone pressure and delay further ejection of fuel into 2 phase sodium.</td>
</tr>
<tr>
<td>ejected fuel at 35 msec</td>
<td>g</td>
<td>13</td>
<td>11</td>
<td>10</td>
<td>The sodium temperatures is ca. 900°C above the cladding temperature at onset of boiling. Heat transfer to clad and structure is a major source of pressure relief in the FCI zone. Smaller condensation coefficients increase the FCI zone pressure and delay further ejection of fuel into 2 phase sodium.</td>
</tr>
</tbody>
</table>

1) All components are ejected proportional to their respective time dependent volume fractions in the cavity.
2) The reactivity contributions are those from all pins in ring 1 + 2 of FFTF.
3) These cases were calculated with a failure pressure of 1900 atm.
Table 4  Characteristic Results from SNR-Overpower Accident Simulations with Different Failure Criteria

<table>
<thead>
<tr>
<th>Failure Criterion</th>
<th>100-day core</th>
<th>Core at Beginning of 117-day Equilibrium Cycle</th>
</tr>
</thead>
<tbody>
<tr>
<td>Failure Time</td>
<td>1.28 sec</td>
<td>1.10 sec</td>
</tr>
<tr>
<td>Failure Position (%Active Height)</td>
<td>Inner zone: 46%</td>
<td>117-day fuel: 57%</td>
</tr>
<tr>
<td></td>
<td>Outer zone: 36%</td>
<td>234-day fuel: 75%</td>
</tr>
<tr>
<td>Failure Melt Fraction (at Midplane)</td>
<td>30%</td>
<td>117-day fuel: ~10%</td>
</tr>
<tr>
<td></td>
<td></td>
<td>234-day fuel: ~5%</td>
</tr>
<tr>
<td>Peak Two-phase Sodium Pressure</td>
<td>80 atm</td>
<td>10 atm</td>
</tr>
<tr>
<td>React. Ramps from Core Voiding</td>
<td>22 $/sec</td>
<td>2 $/sec</td>
</tr>
<tr>
<td>Begin of Fuel Sweepout (rel. to failure time)</td>
<td>15 msec</td>
<td>20 msec</td>
</tr>
<tr>
<td>Fuel-motion Feedback after Fuel Sweepout from Channel 2</td>
<td>+ 10 $/sec*</td>
<td>- 1 $/sec</td>
</tr>
<tr>
<td>Max. Net Reactivity</td>
<td>&gt;1$</td>
<td>0.72$</td>
</tr>
<tr>
<td>Max. Rel. Power</td>
<td>&gt;50</td>
<td>6</td>
</tr>
<tr>
<td>Final State of Core</td>
<td>Transition to disassembly</td>
<td>Subcritical, but accident termination not assured</td>
</tr>
</tbody>
</table>

* The reactor is super-prompt critical at this time. This causes further rapid ejections of molten fuel into the midplane area, which compensate the negative reactivity feedback from fuel sweepout.
Fig. 1  Cross Section of SNR 300

Fig. 2  Power and Net Reactivity vs Time, 100 Day Core, Melt Fraction Criterion
Fig. 3  Power and Net Reactivity vs Time, Equilibrium Core, Rupture Strain Criterion

Fig. 4  Channel 2 Fuel Motion and Sodium Void Feedback, Equilibrium Core, Burst Pressure Criterion
Fig. 5 Power and Reactivity vs Time, Equilibrium Core, Burst Pressure Criterion

Fig. 6 Lower Coolant Plenum Pressure vs Time, Equilibrium Core, Burst Pressure Criterion
ABSTRACT

In the GBR-4 design of a gas-cooled fast reactor, emphasis is placed on using to advantage the single-phase character of the coolant. Specific features of the protective systems are fail-safe neutron absorption, permanently operating auxiliary supplies and atmospheric pressure cooling. For transient analysis a computer program is applied using a point reactor model for heat transfer and neutron kinetics. A dynamic steam generator code has been employed to describe secondary side effects. Anticipated disturbances in operation are reactivity insertion, loss of supplies and loss of coolant pressure. For these accidents, failures in reactor protection are postulated in decreasing order of probability. The consequences are analysed and discussed. The results lead to the conclusion that, owing to the inherent characteristics of the system and to the specific design, occurrences are well within the limits of a typical probability-release criterion.
GBR-4 PROTECTION SYSTEMS — FAILURES AND THEIR CONSEQUENCES

1. INTRODUCTION

The total effort devoted to alternative coolant fast breeders has always been modest compared with world-wide expenditure on the sodium reactor. Interest in the gas-cooled fast breeder has, however, grown over the last decade, mainly for two reasons. Firstly, valuable experience in gas reactor technology has been acquired in Europe and the US and this can be used to advantage in the economic development of fast reactors. Recent progress in introducing the high temperature reactor in the US has provided an additional incentive to extending available helium technology to a future fast breeder. Secondly, the single-phase character of the gaseous coolant eliminates the problems associated with a coolant phase change, such as reactivity effects, heat transfer discontinuities and vapour explosions. The fact that a potential void formation is disregarded facilitates safeguards engineering and helps to make safety predictions more reliable.

During the early sixties, several groups in Europe and the US worked on the design and performance aspects of the gas-cooled fast reactor. The prospects offered by this concept were presented at the Third Geneva Conference in 1964/1/. In the following years activities in this field were increased and more clearly defined programs set up which were based on contributions from research centres, industry and utilities in different countries/2/. In 1969 an initiative on the part of industry led to the creation of the Gas Breeder Reactor Association (GBRA), which presented the results of a design and assessment study in 1972/3/ and is at present about to complete a preliminary safety analysis report.

The object of this paper is to demonstrate the safety potential of the GBR-4 fast breeder by presenting the system's response to certain abnormal conditions involving failures of the protective installations. The principles of the main safeguards are reviewed only briefly and for details reference should be made to the complementary paper 10/6 to be presented at this conference by J. Chermanne. The calculation methods used in the analysis are described and conceivable failure modes and associated probabilities enumerated. A discussion of the plant's transient behaviour illustrates the possible consequences of anticipated and postulated faults.

2. LAYOUT OF NUCLEAR STEAM SUPPLY SYSTEM

The GBR-4 is a 1200 MW helium-cooled fast reactor. Pu-U oxide fuel is clad in stainless steel tubes (pins) from which fission gases are continuously vented, thus keeping the pressure in the pins slightly below the external coolant pressure. Seven fuel subassemblies (pins spaced by grids and wrapped in hexagonal boxes) stand on one core support foot/4/. Thirteen subassembly positions are reserved for absorber elements, divided up between two independent systems.
The GBR-4 nuclear steam supply system, together with three emergency decay heat removal loops, the fission gas venting and trapping system and the helium purification plant, are integrated in a prestressed concrete pressure vessel (PCV) (Fig. 4 in [5]). Helium at 90 bar flows upward through the standing core and down through the helical steam generators, housed in six pods around the central reactor cavity. Vertical-axis coolant circulators are installed in the lower part of the steam generator pods. The centrifugal impeller is driven by a speed-controlled induction motor. An auxiliary drive in the form of a pony motor runs on the same shaft.

The three emergency circulators and coolers are housed in pods of smaller diameter between the main pods. The lower part of these smaller pods is separated from the upper part by a thick concrete slab and contains the fission gas trapping and the helium purification system. A small vault below the reactor houses the absorber rod drives and the fission gas collecting and monitoring devices. These four cavities are designed for the full coolant pressure but normally work at 1 bar.

Off-load refueling is performed by two machines through two penetrations in the PCV upper slab. The pantograph machine, which can cover all the core, blanket and reflector positions, handles the assemblies inside the reactor vault. The refueling machine places the assemblies in and removes them from one single position in the vault.

The nuclear steam supply system (NSSS) is surrounded by a double-shell containment building. The inner (steel) shell serves as a leaktight barrier in the event of coolant or activity release from the NSSS. The outer (concrete) shell protects the NSSS against external influences. The interspace is back-vented to the inner shell. A list of the NSSS parameters is given in [5].

3. REACTOR SAFEGUARDS

3.1. Inherent Characteristics
The GBR system possesses certain inherent features which are favourable in transient conditions and back up the engineered safeguards in their function of protecting the reactor. These features stem from the single-phase character of the coolant, which not only eliminates the problems associated with a void formation but also enables full advantage to be taken of the large over-temperature margin available with steel-clad fuel. This margin provides scope for power reduction by Doppler and core expansion and also for absorbers to be released by melting devices and inserted by gravity alone. The high coolant over-temperature, which is tolerable in an emergency, favours natural convection.

Other inherent features of beneficial safety relevance are the low reactivity worth of the coolant and the low excess reactivity of a system with a high internal conversion ratio [6]. The transparency of the coolant ensures easy inspection and maintenance and the inertness of the helium prevents chemical reactions.

3.2. Reactor Shutdown Systems
For reactor shutdown, two independent systems are provided. These differ with respect to the design of the drive mechanisms and absorbers and each one can keep the cold reactor subcritical. All the absorbers are actuated from the bottom of the reactor and, in the withdrawn position, are located above the core. They are inserted by gravity in the event of a trip. The shutdown rods in the second system, besides being able to be tripped by the reactor safety system, are also equipped with a melting device which would ultimately release the absorbers should the temperatures approach dangerous limits. The absorbers are inherently safe against ejection [5].
3.3. Helium Density
The helium density is maintained by the prestressed concrete vessel (PCV). Large penetrations are closed by concrete plugs. These are fixed to the vessel by unitized and redundant means, so that the prompt opening of a large penetration cross-section is of acceptably low probability. In the event of gaps appearing between the plugs and the PCV owing to prompt and complete failure of double seals, a safe depressurization rate is guaranteed. Other conceivable imperfections in the pressure envelope cause lower leak rates. In the event of a PCV depressurization, the containment building would provide an equilibrium pressure of between 2 and 3 bar, depending upon the temperature.

3.4. Helium Circulation
Forced helium circulation by main blowers is backed up by an auxiliary power supply to pony motors running on the main blower shaft (Fig.1). The six pony motors are fed from three small steam turbine generators supplied from the main boiler live steam headers via a drum. This auxiliary supply system is in permanent operation so that the pony motors always contribute a small fraction of power to the main impeller drive. Should the main supplies be lost, the system would continue to run on reactor decay power and drive the main circulators at reduced speed. If the helium density were to fall, the pony motors would speed up.

In addition to the auxiliary drive of the main circulators there are three independent emergency cooling loops (Fig.1). The circulators for these are of different design and are especially suited to deliver a large volumetric flow at low coolant density. They can thus remove reactor decay heat adequately at atmospheric pressure. The circulators have motor drives. Each one is supplied from a diesel generator via a static frequency converter which controls the circulator speed according to a constant electric power.

Natural convection of pressurized helium through the emergency loops is capable of removing the decay heat from the reactor and thus keeps the temperatures within acceptable limits.

3.5. Heat Sink
The main heat sink provided by the steam generators is backed up by an auxiliary feed supply. Auxiliary feed pumps are part of the above-mentioned auxiliary supply system and continuously contribute a small fraction to the boiler feed. Together with the pony motors on the main circulators these feed pumps guarantee an uninterrupted heat removal chain should the main supplies be lost.

The emergency cooling loops are equipped with pressurized water coolers. Water circulation and heat removal from high air coolers are by natural convection.

The emergency decay heat removal system is not normally in operation and a core bypass through the three loops is prevented by check valves (flaps) which operate automatically as a result of changes in the pressure difference across them. Should normal plant operation be disturbed, the emergency system would start up and run on stand-by. It would automatically take over should heat rejection through the main loops fail to an unacceptable extent. The main loops are also equipped with check valves to stop back-flow.

4. METHOD OF CALCULATION
4.1. Reactor
Faults in the GBR primary system have been analyzed using the code PROFUSION-GAS. This is adequate for calculations of relatively slow transients which enable the reactor to be represented by a single point model. This method is particularly applicable to a GBR, where the coolant transit time through the reactor is small compared with the time constant of fuel to coolant heat transfer.
The point neutron kinetics equations are solved by means of a quadratic pro-
gressive approximation method \(^9\). The point model for calculating the tran-
sient coolant temperature uses a lumped fuel and cladding time constant,
declared as the ratio of the fuel and cladding thermal inertia over the global
heat transfer coefficient from fuel to coolant. The transient fuel temperature
is calculated in a similar way.

At certain time increments the code gives the temperature profiles at 18 axial
positions in a representative core subchannel. This calculation uses pseudo-
steady-state methods. The concept of the conductivity integral is used for
the fuel, while the gap and cladding have constant conductance. The convective
heat transfer is a function of the local Reynolds number and local coolant
thermodynamic properties. Different Stanton/Reynolds correlations are used for
the smooth and the roughened part of the fuel pin. Only turbulent flow condi-
tions are considered.

For steady-state nominal power, the code gives the spatial temperature distri-
bution in the cylindrical reactor core with two radial zones, upper, lower and
radial blankets, divided into a mesh of a maximum of 30 axial nodes and 28
radial regions. The radial distribution of the coolant flow is assumed to be
proportional to the power and this corresponds closely to the distribution
which will obtain in the gagged core.

The driving functions of the transient are reactivity, coolant pressure, coolant
mass flow and coolant temperature at the core inlet. The two components of the
reactivity driving function are the direct insertion and the feed-back from
Doppler, thermal expansion and coolant density. The coolant pressure is a func-
tion of the temperature and the rate of loss. The coolant mass flow depends on
the circulator speed, the circulator and circuit characteristics and the coolant
density. The core inlet temperature function is an output from the steam gene-
rator code PANDYNE. The decay heat curve used (Fig.6) follows a recommenda-
tion in \(^{10}\). It is higher than those given in other references and guarantees that
the results will be on the safe side. Trip levels are generally assumed to be
at 115\%. A block diagram of the code is shown in Fig.2.

4.2. Steam Generator
The behaviour of the steam generator determines the core inlet temperature
transient. Water/steam side transients have a bearing on the load on the
boiler and also on the operation of the auxiliary supply system. The steam
generator dynamics have been calculated with the code PANDYNE and the cold gas
temperatures then fed into PROFUSION in an iterative procedure. PANDYNE is a
non-linear digital program for the analysis of boiler dynamics. The adequacy of
the results has been demonstrated by tests on several once-through boilers \(^{11}\).
The code treats the medium in single-phase regions as incompressible and it is
only in the evaporator that the specific volume is variable in both time and
space. The thermodynamic properties of water/steam are given by functions based
on the VDI tables and are taken to be constant over a section (economizer,
evaporator, etc), but vary in time where necessary.

5. DISCUSSION OF TRANSIENTS
The probability figures used are based on a preliminary reliability assessment
\(^{12}\) or are taken from the literature. The melting point of the clad material
AISI 316 L is above 1400°C. The safety limit for short term hot-spot tempera-
ture at low flows can be set at 1300°C, particularly since the vented pin
concept and a low-oxidizing coolant are used.
5.1. Reactivity Insertion

In the GBR there is no mechanism for a large reactivity insertion since absorbers cannot be ejected and insertion rates associated with a fast depressurization are smaller than 1 \( \text{dl/sec} \). Thus only small ramps (e.g., those due to unintentional rod withdrawal) need to be considered and these allow convenient scope for protective action. A control rod withdrawal at maximum speed produces a ramp of 5 \( \text{dl/sec} \). Should, in the event of an interlocking failure, all seven control rods be moved simultaneously, the ramp is about 35 \( \text{dl/sec} \). Fig.3A shows power transients from nominal condition up to reactor trip on power and, in dotted lined, the continuation in the event of a trip failure. Ramps of 70 \( \text{dl/sec} \) and 2 \$/sec have been included to demonstrate the safety margins. Fuel melting would not occur in any of these cases provided the reactor is tripped on power. The effectiveness of other trip levels is indicated in a table (Fig.3B).

Probabilities of 10\(^{-2}\) and 10\(^{-3}\)/ry are attributed to the 5 and 35 \( \text{dl/sec} \) rates respectively. With two independent trip systems and a reliability of single trip signals of 99% there is, in the case of single rod withdrawal, an extremely low probability of the fuel pins being at risk. For the seven-rod withdrawal the highest rated pins may be at a risk of 10\(^{-7}\)/ry since the failing power scram entails some fuel melting. Reactivity insertion has no significant safety implications in the GBR.

5.2. Loss of Supplies

Should the main power and/or feed supplies be lost, the reactor is tripped and the heat dissipated via the auxiliary system, which is designed on a one-out-of-three basis. The assumed probability of the grid being lost is once per reactor year. There is an 86% chance that the auxiliary system will be fully available for decay heat removal. All the temperatures drop rapidly and are maintained at levels considerably below nominal except for the core inlet temperature, which rises slightly.

The probability of transfer operations to the auxiliary supply system failing in two units and only one-third of the system (i.e., one auxiliary unit with two main NSSS loops) being available for decay heat removal has been assessed at 7 x 10\(^{-3}\)/ry. The rapid temperature drop following the scram is a result of the small core heat capacity together with the considerably different time constants of circulator rundown and power reduction (Fig.4). The pressure drops accordingly. The circulators run down in parallel until the point where two out of six continue to run on the auxiliary supply and the other four idle down to a complete stop. The coolant mass flow falls off once again, causing the clad and gas temperatures to rise again, but they remain below nominal values. Back-flow through the inoperable loops is prevented by the non-return flaps. As for the steam generator, the displacement of the boiling region and transient of the steam and tube temperatures at the superheater outlet do not present any safety problems.

The heat removal through auxiliary supplied main loops may fail completely and the probability of this occurring has been assessed for a random failure distribution (not common mode fault) at 1 x 10\(^{-4}\)/ry. In this event the emergency system would take over automatically and remove decay heat via the air coolers on the containment roof. Fig.5 shows the transients for the case in which the emergency units are called upon immediately after reactor trip and only two of the three units manage to start up. Here also, the temperatures remain well below the nominal values.

The reliability analysis for the GBR-4 decay heat removal systems has revealed a probability of 3 x 10\(^{-7}\) for the total loss of all forced coolant circulation. Should this situation occur, natural coolant convection would set in through the
emergency loops, in which the coolers are in a favourably high position relative to the reactor core. About 2% of nominal mass flow through the core can be maintained. For some minutes the clad hot-spot temperature may reach values of 1100-1200°C, while maximum gas temperatures would not exceed 900°C (Fig.6).

The discussion of auxiliary and emergency decay heat removal from the pressurized reactor demonstrates that fuel and coolant temperatures are generally maintained at very low levels. The primary coolant circulation machinery is designed for adequate operation with the coolant depressurized, so that the installed power is more than adequate for the conditions discussed above. Only in the very remote event of a total loss of forced circulation would fairly high temperatures be recorded, but these are well below the safety limit as set out above. The core will remain in a coolable configuration and there will be no abnormal activity release from the plant.

5.3. Loss of Coolant Pressure

The design basic depressurization rate (time constant 200 sec) has been associated with a probability of 10^{-3}/ry \cite{13}. During this accident coolant is discharged from the primary circuit within 12 min. Containment equilibrium pressure depends upon the temperature and thus changes with time from 3 to 2 bar. If the normal reactor control system is assumed to be ineffective, the power rises slightly owing to the positive reactivity feedback from coolant depressurization (Fig.7). The temperatures increase with increased power and reduced coolant density until the reactor is scrammed on high temperature and the circulator main motors and main feed pumps are tripped. The circulators run down on pony motor drive and speed up again with decreasing coolant density. Clad hot-spots reach their maximum of 870°C shortly after depressurization is completed. In the steam generators the boiling region moves slightly downward. The live steam temperature follows the gas temperature closely. With the reactor power control system in operation temperatures would remain fairly constant during the first 33 sec and the system would be tripped on low pressure. The clad temperature margin against postulated faster depressurization rates is shown in Fig.8.

Should one-third of the auxiliary system fail upon depressurization (probability < 2 x 10^{-4}/ry), maximum temperatures would exceed those for full system availability by only 20-30°C. This effect is due to the favourable circulator characteristic and to the 50% higher decay power per unit, which tends to overspeed the circulators and increase the feed supply.

There is a chance of 7 x 10^{-6}/ry that the emergency system will be called upon in a depressurization accident and a two-out-of-three availability is required during the first three hours to keep the clad hot-spot temperatures below 1200°C. The probability of the required minimum of machinery failing and hence causing significant clad melting is assessed at 2 x 10^{-8}/ry. The long-term reliability of the emergency system is discussed in \cite{14}.

Although the fission gas venting system can be designed for stable operation in pressure transients, some activity release is to be expected as a result of a depressurization. This is calculated on the assumption that 1% of the fuel pins have imperfect cladding and 1% of these contain vent line faults which would prevent the fission gas venting system from operating effectively during a rapid depressurization. The activity inside the containment and the small accumulated release to the atmosphere are shown in Fig.9.

Earlier studies suggest that containment engineering standards may not justify reliance on an equilibrium pressure as a vital feature in reactor decay heat removal \cite{13}. With the GBR-4 emergency circulators, decay heat can be removed at atmospheric pressure. Two out of three loops are adequate if the loss of
equilibrium pressure takes more than two hours. In a postulated fast depressurization, in which the containment is completely incapable of retaining the coolant, all three emergency loops are required for adequate cooling. The emergency loops take over when the pressure falls below 2 bar and maximum clad temperatures are kept below 1200°C (Fig.10). This is well below the melting point and the core geometry would be safely maintained. The associated activity release is about 3300 Ci $^{131}$I equivalent and since the probability of this occurring is not higher than $10^{-6}$/ry, the event is more than one order of magnitude lower than the limit proposed by Farmer $^{[15]}$.

5.4. Failure to Scram
With two independent and adequately designed reactor trip systems, the probability of the reactor failing to scram upon loss of supplies is supposed to be no higher than $10^{-8}$/ry. This event would not normally be regarded as a design basis; it is discussed here merely to demonstrate the effect of a large temperature margin on reactivity feedback and also because at reasonable extra cost the GBR-4 auxiliary supply system powered by reactor heat can be designed to cope with this accident. Reactivity feedback from Doppler, axial and radial core expansion and coolant density reduces the reactor power to 46% (Fig.11A). If the boilers are adequately fed, they will withstand the high gas temperature for some hours. When the transient is stabilized, this would provide scope for alternative shutdown means.

With a scram failure upon rapid loss of pressure the transients do not stabilize but sufficient time is available for the fusible trip devices to release absorber material (Fig.11B). The probability of such a failure coincidence is calculated to be below $10^{-10}$/ry and can thus be disregarded.

6. CONCLUSION
The GBR-4 safety system turns to advantage the inherent characteristics such as the small potential reactivity insertion and the large over-temperature margin which allows the negative reactivity feedback to reduce reactor power strongly.

With gravity insertion the reactor shutdown system can safely terminate the conceivable reactivity transients and the mismatch of power/coolant flow. The reliability of the absorber release is improved by fitting fuses which can become effective on account of the large coolant temperature margin.

The performance of the decay heat removal systems is adequate both in the pressurized state and at atmospheric pressure. For low probability failure coincidences the design criterion is to keep the clad temperature sufficiently below the melting point so as to maintain the core geometry. High reliability of decay heat removal is achieved by permanent operation, diversification and unitization of the cooling system and by using the natural convection potential.

With regard to these features a gross fuel melt-out has an extremely low probability. The GBR-4 design is therefore not equipped with special core-catching devices. However, studies of improved bottom cooling systems are being performed which would enable molten fuel to be contained and allowed to solidify.

7. ACKNOWLEDGMENTS
The authors acknowledge the work of their colleagues in the GBRA Study Group, in particular J. Chermanne, J.P. Demaret, C. Oppenheim, F. Presciuttini, P. Romita, C. Sacriste, P. Van Asbroeck, D. Van Deyck and J.M. Yellowlees. Thanks go to the GBRA member companies and their staff for their kind support, in particular U. Bachmann and K. Bochsler of Sulzer Brothers Ltd who performed the dynamic boiler calculations, and A. Renard of Belgonucléaire SA, who was a source of valuable advice about the PROFUSION code.
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FIG. 1 - HEAT REMOVAL SYSTEM SCHEMATIC

FIG. 2 - BLOC DIAGRAM OF "PROFUSION" CODE
FIG. 3 REACTIVITY INSERTION

A  POWER TRANSIENTS
B  EFFECT OF SCRAM LEVELS

**TABLE 1**

<table>
<thead>
<tr>
<th>SCRAM INITIATION</th>
<th>POWER 115%</th>
<th>TEMPERATURE 115%</th>
<th>PRESSURE 115%</th>
<th>FUSE 750°C MELTING POINT</th>
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<tr>
<td></td>
<td>(s/s)</td>
<td>(s/s)</td>
<td>(s/s)</td>
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</tr>
<tr>
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<td>3.5</td>
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<tr>
<td>0.70</td>
<td>2360</td>
<td>10</td>
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*PERCENTAGE OF MOLTEN CROSS-SECTION AT MAXIMUM POWER DENSITY*
FIG. 4  LOSS OF MAIN POWER SUPPLY. 1 EX 3 AUXILIARY SUPPLY SYSTEMS AVAILABLE. PRIMARY AND SECONDARY SIDE TRANSIENTS
FIG. 5  LOSS OF ALL AUXILIARY SUPPLY SYSTEM
DECAY HEAT REMOVAL BY 2 EX 3 EMERGENCY LOOPS

FIG. 6  LOSS OF ALL FORCED CIRCULATION
DECAY HEAT REMOVAL BY NATURAL CONVECTION
FIG. 7 - DEPRESSURISATION TO CONTAINMENT EQUILIBRIUM PRESSURE
(TIME CONSTANT 200s) PRIMARY AND SECONDARY SIDE TRANSIENTS
FIG. 8 - COMPARISON OF CLAD HOT SPOT TEMPERATURES FOR DIFFERENT POSTULATED DEPRESS. RATES

FIG. 9 - ACTIVITY RELEASE ASSOCIATED WITH COOLANT DEPRESSURISATION

FIG. 10 - DEPRESSURISATION TO ATMOSPHERIC PRESSURE - (TIME CONSTANT 200s)
FIG. 11 - FAILURE TO SHUT DOWN

A  LOSS OF MAIN SUPPLIES
B  DEPRESSURISATION ($\tau = 200$ s.)
SAFETY ANALYSES OF A 300-MW(e) GAS-COOLED FAST BREEDER REACTOR

D. Buttemer, H. Chi, W. Reinsch, and A. Torri
General Atomic Company

ABSTRACT

Extensive safety evaluations of the 300-MW(e) GCFR demonstration plant to determine the response to anticipated plant transients and to postulated accidents are summarized in this paper. Cases presented include normal reactor trip, loop isolation, control-rod withdrawal accidents, accidents involving malfunctions in the circulator control valves, and depressurization accidents. The results are very encouraging with respect to the capability of the GCFR to safely accommodate the spectrum of cases investigated without exceeding fuel or coolant-system safety limits.

INTRODUCTION

The initial studies of the Gas-Cooled Fast Breeder Reactor (GCFR) were initiated in 1962 at General Atomic Company and the GCFR program has continued development under combined funding from General Atomic, a group of supporting electric utility companies, and the USAEC. The GCFR technology is extensively based on the gas-cooled reactor technology developed for the High-Temperature Gas-Cooled Reactor (HTGR) program. A conceptual review of the safety and licensing characteristics of a 300-MW(e) demonstration plant design has been going on since 1971. The safety related criteria and design features of the GCFR are discussed in a companion paper at this conference. (1)

The configuration of the reactor and its associated reactor coolant circuit components contained within a prestressed concrete reactor vessel (PCRV) are shown in Fig. 1. The entire reactor coolant system is contained within the PCRV. All interior surfaces of the PCRV have a leaktight steel liner, and the large penetration closures over the central reactor cavity and the three peripheral steam generator cavities are made of concrete. These concrete closures are joined to the vessel liner by seal welds and are provided with a structurally independent flow restrictor to limit the coolant depressurization rate in the event of a seal failure. The conservative design of PCRVs with a redundancy of prestressing members that are inspectable and replaceable precludes a gross failure of the pressure vessel. To limit the consequences of a depressurization accident, the PCRV is enclosed within a secondary containment building. This containment building ensures a minimum coolant backpressure for core cooling and provides an additional barrier for fission products that potentially could be released following a primary system depressurization.

The reactor coolant system consists of three main loops, each with an independent steam generator and turbocirculator, and three auxiliary core-cooling loops, each with its own electrically driven centrifugal circulator and heat-removal system. The auxiliary loops are used as backup for the main loops for
Fig. 1 Cutaway view of 300-MW(e) GCFR demonstration plant PCRV
long-term shutdown cooling. Both the primary coolant loop and the auxiliary coolant loop systems are housed in vertical cavities in the PCRV wall that surrounds the reactor cavity. The helium coolant flows downward through the core, where it is heated to a temperature of 1022°F at a pressure of about 1250 psia. The coolant flow is then upward around the radial thermal shield, radially across to the three steam generator cavities, then downward across the tube banks of the helically coiled, once-through counterflow steam generators. After leaving the steam generators it flows up again around the boiler shells and then to top-mounted circulators, from which the coolant gas is discharged to the reactor inlet plenum at a temperature of 613°F. The three main circulators, of the type developed for the HTGR, employ a single axial compression stage driven by a single impulse steam-turbine stage. Auxiliary circulation is provided by electrically driven centrifugal circulators.

The reactor core is made up of 265 hexagonal fuel and radial blanket elements containing the fuel and blanket rods. The elements, which are approximately 11 ft in total length and about 6.6 in. across flats, are rigidly attached to a top-mounted monolithic grid plate. The elements are clamped to the grid plate solely at their "cold" ends and no additional core restraint is provided. Core refueling is done from below.

Each fuel element contains 270 fuel rods that are similar to LMFBR fuel rods except for two fundamental differences. First, the cladding surface is roughened over the lower 75% of the active core region to improve the surface heat transfer. Second, the pressure in the fuel rods is maintained slightly below that of the reactor coolant by collective venting to the helium purification system at the circulator inlet pressure. This pressure equalization relieves the cladding from the mechanical stresses due to external gas coolant pressure and internal fission-product gas pressure. In addition, this system limits the release of activity from failed rods to the reactor coolant and provides a means for detecting and locating fuel elements with failed cladding. The coolant flow in each element is orificed so that essentially the same hot-spot cladding temperature is reached in each element.

The control-rod drives are located above the reactor cavity closure. Normal operation of the reactor is provided by 21 control rods, each having a worth of $0.85. These rods compensate for burnup and other reactivity effects and can shut down the reactor from any operating condition. In addition, there are 6 shutdown absorber rods, each having a worth of $1.60, which are a backup shutdown system that is capable of independently shutting down the reactor. Both sets of rods are automatically inserted into the core during reactor trip. The 21 control rods are inserted by a spring-assisted gravity motion and the 6 shutdown rods, which are fully withdrawn from the core during normal operation, are driven into the core by the shutdown-rod drives at a speed tentatively set at 300 in./min.

Feedwater supplied to each of the three steam generators flows upward through the economizer, evaporator, and superheater sections. The superheated steam then flows through two parallel circulator-turbine control valves located in each loop, through the circulator turbine, and then through the resuperheater. The resuperheated steam from each loop is then combined and flows to the turbine generator unit.

Several computer programs have been developed at General Atomic to analyze the plant response to anticipated transients, operational transients, and accidents. Several of these transients are discussed below.
SYSTEM RESPONSE FOLLOWING A REACTOR TRIP

The system response following a reactor trip with the shutdown control system functioning normally is indicated in Fig. 2. The core outlet temperature, the maximum cladding hot-spot temperature, and the core helium mass flow rates are shown for the first 30 min following the reactor trip. The power decreases rapidly to the decay heat level due to the insertion of the control and backup shutdown rods. The helium flow rate decreases more slowly, however, due to the closure rate of the circulator-turbine large control valve and to the inertia of the turbocirculator. The effect of this temporary overcooling may be observed in the initial drop of both the core outlet temperature and the maximum cladding hot-spot temperature. The small peaks of the cladding and core outlet temperatures at approximately 5 sec are due to the release of the stored thermal energy in the fuel. Thereafter, the mild temperature transients are primarily due to the circulator-turbine small control valve controller action. As the design of the controllers has not been finalized, these results should be considered as preliminary.

MAIN-LOOP ISOLATION WITH PROGRAMMED LOAD REDUCTION

The detection of a fault condition within a cooling loop automatically initiates the isolation of that loop, which consists of an orderly circulator shutdown and a programmed load reduction. The main-loop isolation valves are self-actuated louver-type valves which close immediately when the helium flow reverses in the isolated loop. The power and temperature transients during a loop isolation have been analyzed, including the effect of a delay in the isolation-valve closure. The effect of bypass reverse flow through the isolated loop with a valve not closed is included in the analysis.

For the case of immediate loop shutdown and isolation, the core flow rate rapidly decreases to 79% in less than 1 sec. The reason the helium coolant flow rate is greater than two-thirds of the steady-state value when only two of the three loops are operating is that a constant circulator power is assumed and that the core pressure drop decreases. If the isolation valve within the shutdown loop fails to close, part of the flow supplied by the two operating loops backflows through the shutdown loop and the equilibrium core flow reduces to 59%. To evaluate the influence of a check-valve closure delay, the core flow was approximated by assuming a linear variation from 100% to 59% in 1 sec and then a linear increase to 79% when the check valve is fully closed. The programmed load reduction corresponds to inserting three control rods at their maximum design speed of 10 in./min. Neutron kinetics with the various feedback mechanisms are included in the calculations.

The results of the analysis for the immediate loop shutdown and isolation ($\tau_C = 0$) and for several cases with check-valve closure times ranging from 2 sec to the limiting case of a complete failure of the check valve to close ($\tau_C = \infty$) are shown in Fig. 3. After an initial rapid increase in maximum cladding temperature due to the flow decrease, the cladding temperature drops because the reactivity feedback and, later, the control-rod motion reduce the reactor power. The most severe transient, a complete check-valve failure, results in a peak cladding temperature of 1600°F, which is well below the damage limit for the stainless-steel cladding.

REACTOR TRIP WITH CIRCULATOR-TURBINE CONTROL VALVES FAILING TO CLOSE

The circulator-turbine control valves are an important part of the shutdown control system because they strongly affect the circulator speed and hence the core coolant flow rate. The circulator-turbine steam flow rate is governed
Fig. 2 Plant response following a reactor trip

Fig. 3 Cladding hot-spot temperature following loop isolation
by two valves in parallel located upstream of the turbine in each of the three main loops. The large control valves are used for plant control during normal operation, whereas the smaller valves, which operate wide open during normal operation, are used to control the steam flow to the circulator turbine after the reactor has been shut down. When the reactor is tripped, the large valves are closed rapidly (when a reactor trip signal and a confirmatory indication from the neutron flux detectors that the reactor power has been reduced are received) and then the shutdown-control system manipulates the small valves to achieve the desired plant transient behavior. The purpose of this action is to prevent overcooling of the core following reactor trip and also to conserve the steam-generator inventory for continued main-loop cooling for a long enough period until the auxiliary boiler steam supply is available. The auxiliary loops are also available as a further backup.

Failure of all large control valves to close upon reactor trip has been identified as a low-probability potential common-mode failure in main-loop shutdown cooling. This accident results in substantial initial core overcooling and rapid reductions in steam generator inventory and pressure. The results of the transient for the first 2 min are shown in Fig. 4. Both the core outlet temperature and the maximum cladding temperature drop rapidly in the beginning of the accident because of overcooling and then increase to their respective peak values of 1000°F and 1310°F as the helium flow reduces. The steam-generator inventory decreases to approximately 50% during the first 50 sec and is maintained at about that level by control actions.

INADVERTENT CLOSURE OF ALL THREE CIRCULATOR-TURBINE LARGE CONTROL VALVES DURING FULL-POWER OPERATION

Following a normal reactor trip, the three circulator-turbine large control valves are rapidly closed to slow the circulator speed and prevent core overcooling. Steam supplied to the circulator turbines through the small control valves is regulated by the shutdown control system. The turbine large control valves are closed only when confirmatory signals indicating both a reactor trip signal and a reduction in reactor power are received. Independent interlock logic is used for each of the three valves, making the possibility of inadvertent simultaneous closure of all three circulator-turbine large control valves prior to the reactor being tripped extremely remote.

Nevertheless, this accident has been analyzed assuming that the initiating event is the rapid closure of the circulator-turbine large control valves while the circulator-turbine small control valves located in parallel remain wide open. This rapid closure of the circulator-turbine large control valves results in a rapid decrease of the circulator speed and consequently a rapid decrease in helium flow rate. Several reactor trip signals are generated during the first few seconds resulting sequentially from low resuperheater outlet pressure, a high power-to-flow ratio, a low reactivity, and a high reactor outlet temperature. The behavior of the GCFR system during this transient, assuming various degrees of protective action, is shown in Fig. 5. The solid lines indicate the response if the reactor trip is initiated by the first signal, which results in a cladding hot-spot peak temperature of 1590°F. The dashed curves indicate the response if reactor trip is initiated by the second trip signal, which results in a maximum cladding hot-spot peak temperature of 1740°F.

CONTROL-ROD WITHDRAWAL

During normal full-power operation, the reactor is in an automatic-control operating mode. It is conceivable that a control rod could be withdrawn inadvertently and continuously because of a malfunction in the automatic controller.
Fig. 4 Plant response following reactor trip with all circulator turbine control valves failing to close.

Fig. 5 Inadvertent closure of all three circulator turbine large control valves.
or by operator error. Therefore, the control-rod system has been intentionally designed with low-worth, low-speed control rods.

The control-rod withdrawal transient has been analyzed assuming that a fully inserted control rod is withdrawn at the maximum design speed of 10 in./min. The temperature and power transients, as well as the reactivity components, for reactor trip on the first signal are shown as the solid lines in Fig. 6. A rod-withdrawal-prohibit signal is received at 45 sec; however, for this analysis the signal is assumed to be ineffective and the control rod continues to be withdrawn. The first reactor-trip signal is received at 61 sec, resulting in peak fuel and cladding temperatures of 4125°F and 1380°F, respectively. Assuming that the first trip signal fails to be received entirely, a second trip signal is assumed to have been received at 86 sec. In order to demonstrate the effectiveness of the backup shutdown system, it was further assumed that the entire primary reactor trip system fails to respond to the second trip signal and that the reactor is shut down by inserting 5 of the 6 backup shutdown rods and results in peak fuel and cladding temperatures of 4610°F and 1465°F, respectively. This case is shown as the dashed line extensions in Fig. 6. The second peak in the cladding temperature is a result of the coolant flow reduction after shutdown. The various reactivity components during the accident are shown at the bottom of Fig. 6, with negative feedback effects compensating for a substantial fraction of the reactivity added by the withdrawn rod.

FORCED WITHDRAWAL OF A CONTROL ROD AT FULL POWER

Each of the two reactor shutdown systems has been designed not only to control the anticipated rates of reactivity insertion but also to maintain the core within the safety-limit envelope during postulated accidents. The most rapid rate of reactivity insertion in the GCFR results from the postulated gross failure of a control-rod housing. Escaping helium could accelerate the control rod upward, forcing the drive motor to rotate at a much higher than normal speed. The control rod could be fully withdrawn in 2.6 sec, which results in an effective reactivity insertion rate of approximately 50cI/sec.

The reactor transient resulting from the forced rod-withdrawal accident has been analyzed. The first reactor trip signal is received at 0.9 sec owing to high reactivity, and essentially no change in temperature or power has occurred in this short time period because the effective excess-reactivity during the first second is very small. If the first trip signal is assumed to fail, the second trip signal occurs at 1.2 sec due to high power. In order to demonstrate the effectiveness of the backup shutdown system, it was assumed that the entire primary reactor trip system fails and the reactor is shut down by inserting 5 of the 6 backup shutdown rods on the second trip signal. This transient combines both the insertion of the largest amount of reactivity and the most rapid rate believed to be possible in the GCFR. Based on these conservative assumptions, the maximum fuel temperature remains 500°F below the melting temperature and the maximum cladding hot-spot temperature does not exceed 1600°F.

REACTIVITY EFFECTS DUE TO STEAM INLEAKAGE

The reactivity effects of steam entry into the reactor coolant have been analyzed for both the fresh fuel with control rods inserted to hold down the excess reactivity and the end-of-equilibrium cycle with no remaining excess reactivity. The only significant mechanism for steam entry into the reactor coolant system is from the steam generators, which operate at pressures significantly higher than the helium coolant pressure. Redundant moisture monitors are provided
at the primary coolant outlet of each loop to detect tube failures and to initiate corrective loop isolation and steam-generator dump actions if the moisture level exceeds the permissible level. The most rapid potential addition of steam to the primary coolant is caused by the offset rupture of a superheater leadout tube. If this accident were to occur, it is estimated that the steam inleakage is limited to about 1000 lb if the steam generator contents are transferred into a dump tank. If the steam generator dump fails, about 7000 lb of steam would leak into the reactor coolant. The reactivity changes for these two cases are both negative, as shown in Fig. 7. The spectral softening that accompanies the entrance of steam into the core changes both the infinite multiplication factor \((k_{\infty})\) and the neutron leakage. The negative reactivity change associated with the decrease in \(k_{\infty}\) is larger than the positive reactivity change associated with the decrease in leakage and results in a net decrease in reactivity. The relative changes in these two opposing factors depend on the concentration and form of absorbers in the reactor.

**Reactivity Effects Due to Seismically Induced Core Movements**

The GCFR core elements are supported within a massive grid plate at their upper end only. A nominal clearance of 1/4 in. is provided between adjacent core elements to accommodate thermal- and irradiation-induced swelling of the element ducts. This design introduces the potential for small core geometry changes during operation. A detailed seismic analysis of the entire GCFR plant was made to determine the vibrational response of the core during an earthquake. Since a site for the GCFR demonstration plant has not as yet been selected, horizontal and vertical ground accelerations of 0.33 \(g\) and 0.22 \(g\), respectively, were assumed. Using conservative damping and subfoundation soil properties, the radial deflection of the representative element averaged over the active core height was calculated to be 0.135 in. In order to put an upper bound on the potential reactivity effects caused by seismically induced core motion, it was assumed that all of the fuel elements move together in an axisymmetric, radially coherent inward and outward fashion, although the actual motion would be expected to be somewhat random in nature. The reactivity amplitudes and frequencies for the eight radial-motion modes were combined, the two dominant modes having frequencies of 7 and 9 cycles/sec. The increase in fuel and cladding hot-spot temperatures, assuming no protective action is taken, were 400°F and 75°F, respectively. Actually, with the assumed core motion, reactor trip signals would be generated immediately after the earthquake begins. The reactivity effects due to this coherent lateral motion of all fuel elements is equivalent to a step insertion of reactivity of 10\(\tau\).

**Depressurization Accidents**

As mentioned earlier, the conservative design of PCRVs, which employ a redundancy of inspectable and replaceable prestressing members, precludes a gross failure of the pressure vessel. As shown in Fig. 1, the PCRV has large concrete closures over the reactor and steam generator cavities. Incorporated in the closures are welded metallic seals that are not normally opened during the lifetime of the plant. These closures also have flow restrictors that limit the effective leak area to 25 in.\(^2\) if the primary seal should fail. A major failure of the seal in the reactor cavity closure would have a low probability of occurrence and constitutes the design-basis depressurization accident initiating event. There are several smaller PCRV penetrations associated with refueling, instrument lines, control rods, etc. The leak area associated with this class of penetrations is on the order of a few square inches but, because there are more of these penetrations and their seals are broken and reconstituted periodically over the life of the plant, their probability of developing a leak may be larger. In order to typify leaks of this category, an analysis of the plant...
Fig. 6 Inadvertent withdrawal of one control rod at full power
response to a 5 in.$^2$ leak area was made. The results of this study are shown in Fig. 8. To show the gradual nature of the transient, the calculations were continued assuming various levels of protective action. The limiting case of no protective action is shown as the dashed line extension in Fig. 8. The cladding hot-spot temperature reaches the damage point after about 10 min. This allows sufficient time for operator-initiated protective action prior to core damage in the highly unlikely situation that automatic shutdown action does not occur.

The design-basis depressurization accident (DBDA) for the GCFR assumes a failure in the seal of the reactor cavity closure.\(^4\) The flow restrictor limits the leak area to 25 in.$^2$ and the depressurization is complete in about 4 min. The system response to the DBDA is shown in Fig. 9. The reactor is tripped after about 4 sec due to low helium pressure. A peak cladding hot-spot temperature of 1450°F is reached at 6 min and the steam conditions for the circulator turbines remain satisfactory throughout the initial 30 min, at which time the circulators would be driven by auxiliary boiler steam. Very little activity will be released to the containment during a depressurization accident since the circulating coolant activity is expected to be very low and the peak cladding temperatures are well below the damage limit. The peak temperature and pressure developed in the containment atmosphere during a DBDA are 18 psig and 290°F and the equilibrium backpressure is 1.8 atm.

Additional depressurization cases have been analyzed, including single failures, variation in leak size, and variation in backpressure. These have shown that there is substantial margin in the capability to handle depressurization accidents in the GCFR.

CONCLUSIONS

The results of the extensive safety evaluations of anticipated plant transients and postulated accidents, which are summarized in this paper, are very encouraging with respect to the capability of the GCFR to handle the whole spectrum of initiating faults without fuel or reactor coolant system safety limits being exceeded.

REFERENCES


Fig. 8 Response to depressurization accident for a leak area of 50 in.$^2$
Fig. 9 System response during design basis accident with leak in reactor inlet plenum
SESSION 16
SELECTION OF SAFETY DESIGN BASES
FOR FAST POWER REACTORS (Panel)

Chairman: C. Starr (EPRI)
Selection of Safety Design Basis of Fast Reactors in the Federal Republic of Germany

D. Smidt
Institut für Reaktorentwicklung
Kernforschungszentrum Karlsruhe

Abstract

The safety design basis of Fast Reactors to a very great extent is determined by design basis accidents. DBA's are described for the SNR-300 and compared to those for LWR's. For future large fast reactors too conservative assumptions with respect to the whole core accident may lead to serious feasibility problems. Three different strategies for the solution are discussed.

1. General Principles

The underlying principle for the safety design basis of nuclear power stations in the Federal Republic of Germany is - as elsewhere - the defense in depth. During the past years this method has been developed as a combination of probabilistic assessment and arbitrary accident postulates, where the probabilistic basis is too weak. To prevent hazards to the public, three types of "barriers" are used (fig. 1).

- safe design and a safety system, to prevent dangerous transients or to bring the station back into safe conditions. For example neither one nor two independent events may lead to an unsafe condition, common mode failures must be excluded etc. Probabilistic analysis is useful to define the relative importance of different accident paths and countermeasures. Combined with this is a quality assurance program.

- Nevertheless certain design basis accidents (D.B.A.) will be assumed x) and must be contained by special consequence limiting safeguards. It must be ensured that the D.B.A.'s envelop all obvious other cases.

- For the redundant counter-measures against the D.B.A.'s the "single-failure-criterium" must be employed; i.e. from unspecified causes one active unit is assumed to fail.

x) A DBA is a very improbable event; in some cases for a new system arbitrary assumptions must be included.

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In this paper I shall confine myself to the design basis accidents and their assessment, since here are still most of the problems. Their present status for licensing SNR will be described, a comparison made to LWR's, and finally some important questions for the safety design basis of large LMFBR's shall be stated (fig. 2).

2. Present Safety Design Basis Accident

The licensing of the SNR has been done completely within the same framework of rules and organisations as for all other nuclear power stations, especially light water reactors. To my opinion this resulted in a very conservative ruling. The main reason is that a decade of LWR-licensing resulted in a kind of scrupulous perfection of the required proofs, which could be met for a new reactor type only by very conservative estimates.

After considering systematically a number of failure modes and operational transients, the following most important design basis accidents were defined as enveloping other smaller and possibly more probable events (fig. 3).

- representative effects from outside
- design-basis earthquake
- sabotage
- primary pipe rupture with vessel drain
- whole core accident.

2.1 Effects from Outside

This category combines accidents from

- air plane crashes
- chemical explosions (e.g. liquid gas tanker collision on the adjoining river).

The probability of the first category can be quite precisely defined. It is determined completely by crashes of military planes and is practically site-independent. Unfortunately the number of crashes in Germany has been large enough to reach the range of the Farmer-curve, if in a crash some important fraction of the fission-product inventory would be spilled. It presently amounts approximately to $10^{-7}$/year and per station, and especially with a larger number of stations it gains importance.

The design basis required for all reactor types is that the containment must withstand without penetration or major failure a pulse load as shown in fig. 4.

Chemical explosions, mainly from ship's collisions on very frequented rivers can only partially be met by traffic regulations and limitations of ship's size and freight. Therefore, a design explosion has been defined. Normally, the requirements will be met by the design provisions with respect to the plane-crash. Design requirements are shown in fig. 5.
2.2 Design-Basis-Earthquake

The design-basis-earthquake has an assumed mean occurrence rate in the range of $10^{-4}$/a, whatever this practically means. For this case a sufficient function of shutdown equipment and decay heat removal must be proven by 2 independent computational methods.

2.3 Sabotage

The general principle (besides administrative regulations) is that all the equipment in any single room of the plant may be put out of order without preventing shut-down and decay heat removal. This "spatial redundancy" naturally also means additional protection against effects from outside.

2.4 Primary Pipe Rupture (fig. 6)

In a loop type plant [1] a double-ended primary pipe rupture will be considered. By some actions of the safety system the pumps have to be shut off and the cover gas pressure decreased, because otherwise the reactor vessel may be emptied below the nozzles and the decay heat removal via the remaining intact loops becomes impossible. For the SNR a failure of some of these actions has been assumed and an additional emergency core cooling system demanded within the vessel. This also serves generally as a diverse decay heat removal system and will be credited in connection with effects from outside and sabotage.

2.5 Whole-Core-Accident

A whole core excursion may be initiated by certain starting events in connection with failure to scram. Possible starting events are (fig. 7)

- loss of heat sink, turbine trip, loss of electrical power
- gas bubble entrainment into the core
- control rod ejection or withdrawal
- cavity by local FCI.

The group of relatively probable starting events is the loss of heat sink, turbine trip and loss of electrical power. In the order of some 100 events of this nature may be expected during the reactor life. If a major activity release must be prevented with a probability of $10^{-7}$ during the reactor life, it must be prevented with

$$\frac{10^{-7}}{100} = 10^{-9} \text{ per demand (fig. 8)}$$

for each of the starting events. At present, it cannot be supposed, that the reactor trip system alone would have the necessary reliability.

The SNR nevertheless has 2 independent trip systems [1] with diverse absorber elements. However, the redundant sensor-transmitter chains for both trip-systems initially were planned in no diverse manner. It is required to have a truly diverse design for the entire chain. It remains to be proven to which extent this is possible.
Therefore, the safe containment of a whole core excursion was required. Besides loss of flow accidents reactivity excursions were analysed [2]. The excursion initiated by a 5 $/$sec-ramp arbitrarily has been chosen for design-basis calculations. The several steps of the accident were evaluated in a very conservative manner. Fuel failure during the excursion leads to a coolant expulsion starting from about the core midplane; fuel sweepout was not considered. On the basis of the resulting 3000 to 4000 MWsec thermal energy, the system had to be designed to withstand a mechanical work of 370 MWsec (fig. 9), where the relevant structures had to be designed according to a detailed pressure-time-history. The decay heat after this accident must be removed, a fuel melt-through prevented. Other whole core accidents, especially those from cooling failures with sodium boiling, were shown to be milder and therefore covered by these requirements.

3. Comparison with LWR's

The design basis criteria with respect to effects from outside, earthquake and sabotage are entirely the same as those for LWR's. The type-dependent design basis accidents, however, must be discussed.

3.1 Pipe-Rupture

The double-ended pipe rupture is the most important DBA of LWR's. It is agreed, that this can be safely coped with an active system, the ECCS. These systems must be diverse in the entire chain and redundant in a completely decoupled 4 x 50 $/o$ or 3 x 100 $/o$ fashion; the single failure criterion will be employed.

For the SNR on the other side the active means of shutting off the pumps and reducing the plenum gas pressure in case of a pipe rupture were not accepted as being reliable enough. (Indeed for the proposed design the diversity of signals transmitting and actuating members was smaller, but not for principal reasons.) Instead the separate heat sink was required. Therefore, in this respect - let aside the comparative probabilities of a pipe rupture in itself - the fast reactor is designed rather to the conservative side.

3.2 Whole Core Accident

The accident may be called: "Transient without scram" (TWS). At least for PWR's, again initiated by a loss of heat sink, electrical power loss, turbine trip, failure to trip may result in a pressure vessel burst. For this reason, recently additional safeguards (relief valves, second fast trip systems) were required for PWR's. In this respect, therefore, both systems now compare. For the fast reactor, however, the low probability of a WCA is credited by permitting some relaxed requirements as for instance larger strains to failure than in case of the LWR-LOCA. It might be worth mentioning that in case of a LWR vessel burst approximately 1000 MWs of mechanical energy may be concentrated into just one single missile, the vessel head, whereas the total mechanical energy is much larger.
3.3 Other Severe Accidents

The safe containment of whole core accidents is taken as an envelope of other severe accidents, which could destroy the core in bypassing the trip systems (cavities by local SFI, certain types of control rod ejections etc.), however, hypothetical these may be. Here the comparison with the LWR with its hypothetical accidents (e.g. vessel burst) becomes truly speculative.

3.4 Incidents within the DBA-Envelope

Within the envelope of the DBA’s, the influence of less severe incidents on the safety potential of the different systems is different. Fuel handling in fast reactors requires more complicated active systems, whereas in the PWR defect steam generator tubes in connection with secondary pipe rupture or pressure relief are somewhat problematic. In general, however, these problems are a matter of cost and not of principle.

4. Selection of Safety Design Basis for Future Large Fast Power Reactors

For the future successful development of commercial large fast power reactors a more quantitative safety design basis must be acquired. The description of the present status with the LMFBR’s and LWR’s as well as has shown, that there still is a combination of quantitative and qualitative arguments.

This situation is unsatisfactory. It is not caused by any inability to define the acceptable risk. To my opinion we do know well enough, "how safe is safe enough", namely probabilities one or two orders of magnitude below the probabilities of commonly accepted natural risks (fig. 9a). There have been many publications on this risk assessment during the last years [3, 5]. In general, in the range of low activity releases all definitions of permissible probabilities must be based on the permissible individual risk of any person involved [3], whereas for large releases the number of involved persons appears in the correlations. In general, however, the Farmer-criterion as first published in 1964 [4] is used in most cases.

The problem, however, is the difficulty to demonstrate sufficiently low probabilities in connection with severe accidents, in particular, if the safe function depends on a very small number of members. We then face the fundamental problem of statistics with a low number of elements. If protection depends on just one unit, this unit must be extremely reliable. The required failure rates of about $10^{-8}$/year are beyond the possibility of statistical verification. A typical example is the pressure vessel of LWR’s. We are convinced, the vessel is safe; and this conviction is based on a conservative strategy of diverse and redundant quality control. The quantitative assessment, however, will be difficult.

With this in mind let us look at the future fast reactors. I should like to discuss 3 different strategies with respect to the most difficult safety design basis, the handling of the whole core accident. These strategies are (fig. 10):

- WCA will be prevented by the safety system
- WCA considered only from sodium boiling (loss of flow, loss of heat sink etc.)
- Highly conservative WCA considered.
4.1 Case 1: Prevention of WCA

The main component to prevent the WCA is the trip system. As shown before, the required failure probability would have to be in the order of $10^{-9}$ per demand. This is a very low figure. What can be done to achieve this goal?

The minimum requirements are: (Fig. 11)

- 2 diverse and completely independent trip systems, with diverse own sensors, diverse signal processors and actuator units, and diverse absorber elements
- 2 diverse trip signals for each system
- ample redundancy for each system (e.g. application of stuck rod criterion)
- frequent and thorough testing for the complete systems (which might be difficult for the absorber elements themselves) testing frequency to be large compared to demand frequency.

It is too early to decide, whether this is a feasible target. However, it must also be proven, that there is no event, which results in a WCA by bypassing the safety system. Here the proof is confronted with the problem of the single member as stated above. Possible bypasses are e.g. rod ejections, local SFI explosions etc. Their probabilities must be in the range of $10^{-7}$ per lifetime, if this is the permissible risk for a WCA. The formal proof again is very difficult. Also presently a lot of research on local faults and local SFI is underway and our present knowledge certainly is too small. A significant reduction of the risk resulting from local faults can be achieved by (Fig. 12)

- a fast and reliable fission product detection
- a reliable local boiling detector
- a reliable subassembly flow and/or temperature control and
- a reliable signal processing and trip system
- avoid rod ejection by design.

4.2 Case 2: WCA from Sodium Boiling only

The next somewhat more conservative strategy is the consideration of WCA's from sodium boiling eventy only. As was mentioned above, WCA's initiated by total power loss, loss of heat sink, turbine trip etc. in case of trip failure begin with sodium boiling. They are much more probable than steep reactivity excursions. If as a consequence consideration of WCA's is restricted to this type, the definition of the design basis becomes easier and a number of advantages may be gained compared to the present situation. (Fig. 13)

- The number of parameters of the extremely parameter-dependent theoretical WCA-models and their complicated experimental verification becomes smaller
- the importance of the SFI and their role as energy converter might be reduced
- besides the Doppler coefficient other negative feedbacks like bowing and fuel expansion coefficients come into action and may be optimized by design.
time may be gained for preventive action between initialization and actual WCA. This might allow for a truly diverse trip action like coolant poisoning to prevent severe fuel slumping.

The problem of safety system bypass has not been eliminated, but has been relaxed, if a certain limited WCA will be contained. To exclude gas bubbles the pressure in no part of the system should be below atmospheric pressure.

4.3 Case 3: Most Conservative WCA

This case will not need much comments. Everybody who knows the present state of the art also knows how widely open the field of possibilities is. The results of accident calculations are extremely sensitive to some parameters, which must be chosen quite arbitrarily, based sometimes only on qualitative guess work. One may make plausible some mitigating effects, but in many cases proof will not be possible.

I would not expect too much difficulties in designing the reactor cavity to withstand rather severe energy releases. The proof for the decay heat removal system and for the top plug might be more difficult, however, and the largest problems will probably arise with the core catcher.

5. Conclusion

I have tried to show that the safety design basis of present fast reactors compares favorably to that of LWR’s. For future larger plants, however, a better definition, especially with respect to the WCA, is necessary. This might even become a matter of general feasibility of large commercial LMFBR’s. I have outlined 3 alternative ways of assessing this problem. I am hoping for a fruitful discussion of these cases by the panel and by the auditory.

If the breeders are to be the solution of the energy problem in the 1980ies, we don’t have much time but have to decide quickly.

References


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Fig. 1
General Safety Design Basis

- Safe design, safety system, quality assurance
- Safe containment of improbable DBA's by special equipment
- Single failure in active components of special equipment

Fig. 2
Contents of This Paper

- Present LMFBR safety design basis
- Comparison to LWR safety design basis
- Safety design basis for large commercial LMFBR's

Fig. 3
Most Important DBA's for LMFBR

- Representative effects from outside
- DB - earthquake
- Sabotage
- Primary pipe rupture with vessel drain
- Whole core accident
Fig. 4

Design Basis against Air Plane Crash

- impact area
  7 m², circular
- normal incidence

- no overall containment failure
- no containment penetration

Fig. 5

Design Basis against Chemical Explosions

Containment must withstand without major failure

maximum overpressure
- incoming wave 0.3 at
- reflected wave 0.45 at
Fig. 6
DBA: Double Ended Primary Pipe Rupture

Requires:
- Shut down of pumps
- Decrease of covergas pressure

To avoid vessel drain below nozzles
- 2 diverse signals
- Diverse and redundant information processing
- Diverse coolers and heat sink

Fig. 7
Causes for Whole Core Accident

- Loss of heat sink, turbine trip, loss of electr. power
- Gas bubble
- Control rod ejection or withdrawal (improbable by design)

and trip failure
- Cavity by local SFI

Fig. 8
Required Reliability of Trip System,
if Only Barrier Against Major Release

- \( \sim 100 \) demands per plant lifetime
  (mainly turbine trip, loss of heat sink, power loss)

- \( 10^{-7} \) per lifetime; acceptable major release

\[
\frac{10^{-7}}{100} = 10^{-9} \text{ acceptable failures per demand}
\]
Fig. 9
Safety Basis with Respect to Whole Core Accident

- 370 MWsec mechanical work
  (critical member top plug)
- Detailed pressure-time-history
- Decay heat removal after accident

Fig. 10
Strategies for Safety Design Basis with Respect to Whole Core Accident

- Principal acceptance of prevention by safety system
- Initiation by sodium boiling only
- Highly conservative approach
Fig. 11

Requirements to Safety System

- 2 completely diverse and independent safety systems
- 2 diverse trip signals for each system
- ample redundancy in each system
- frequent testing possible for complete system

Fig. 12

How to Avoid Safety System Bypass

- fast reliable fission gas detection
- reliable local boiling detection
- reliable subassembly flow control
- elaborated information processing
- avoid rod ejection by design

Fig. 13

Whole Core Accident by Sodium Boiling Only

- smaller number of parameters (slumping!)
- SFI less important as energy converter
- employ other negative reactivity coefficients beside Doppler
- gain of time
1. INTRODUCTION

The safety of fast reactors designed and built to date is real—it is not hypothetical. Reactor plants are designed to prevent faults from occurring and to protect against faults even if they should occur. Moreover, they have extra capability in heat removal and containment that provides an added margin of safety even beyond that of the multiple protective systems and devices.

However, these plants also have been designed in one way or another to accommodate certain hypothetical accidents chosen either to evaluate the extra margin inherent in the design, or to define extra margin by the addition of specific safety features. These hypothetical accidents have ranged from very low probability events chosen to extend the normal failure mode analysis into the hypothetical range, to non-mechanistic and even non-physical conditions. This paper is aimed at a discussion of the value of this hypothetical safety, its possible consequences and a suggested rational alternative to ensuring the health and safety of the public.

Safety design in this context is associated ultimately with the design of containment of fission products. Therefore, this paper will confine its attention to this subject, rather than extending the discussion to component safety. The design bases and previous experience from existing liquid cooled fast reactors is discussed to illustrate the application, use, and consequences of hypothetical safety. The conclusion of the paper is, that by the application of a more balanced safety approach to a design, the real safety of the plant can be improved by a better assignment of priorities in design and in development activities and by attention to the functional requirements within the plant and the addition of suitable standard margins.

An alternative approach to safety engineering is recommended which hopefully avoids the pitfalls of hypothetical safety evaluations and design while providing properly for the health and safety of the public.

2. PREVIOUS DESIGN BASES

In discussing previous design bases it is necessary, because of the small number of plants involved, to extend discussion to include a plant on which a considerable design and safety effort was expended despite the fact that it was never built; FARET, and to include plants whose designs were initiated a decade ago and which are now just coming to fruition. Despite the fact that many of these plants are relatively small compared to coming 1000 MWe designs, it is still illustrative to review their ultimate capability criteria and the results of the imposition of these criteria.
Table 1 presents a summary of plant characteristics ordered according to criticality date. The increase in size, and transition to mixed oxide fuel is evident. Table 2 presents the containment design bases or design evaluations for these same plants.

One clear trend is noticeable in the chronological list of Table 2. The accidents for which the ultimate capability of the plant are evaluated have progressed from the non-physical to the mechanistic. In earlier plants an arbitrary rearrangement of the fissile inventory into a more reactive configuration was assumed using gravity to impose a time scale upon the rearrangement. No physical initiation was assumed. Later (for SEFOR) a more realistic assumption of sequential slumping of annular zones of the reactor was used. Still later, very low probability events were selected for initiators and in doing so, it was necessary to arbitrarily assume that protective systems failed. This latter assumption is still used today (for example in FFTF) despite the redundancy, independence, and partial diversity applied in the design of the scram systems. However, accident initiators have become more realistic; a loss of coolant from the system giving way to a loss of cooling.

Figure 1 chronologically shows the change in the size of the calculated energy release normalized to unit plant power output. The effect of increasing realism in the calculations is shown by the decrease in energy release from greater than 10 full power seconds to releases of the order of 0.2 full power seconds. A good illustration of the change is that the later comprehensive calculation of the hypothetical energy release for FERMI was reduced by a factor of 5. This reduction was due to an improvement in calculational techniques and in an understanding of the phenomena involved. The MARS code was available in 1967.

It should be added that the increasing size of the plants considered will also have the effect of reducing this normalized energy release since, in larger plants, core dispersal feedback occurs at relatively lower energy levels. However, this effect is small compared to the changing approach to the accidents considered (e.g., compare FERMI & FFTF).

(Since events which resulted in core disruptions did not necessarily provide sufficient bases for the design of the external containment shell, it was also necessary to assume a sodium fire. This has been done with varying degrees of realism. Generally, such sodium fire calculations have simply evaluated the margins available in a building designed to provide environmental protection and to have a very low leak rate. Thus, most outer containments selected for size and ease of fabrication and erection have an inherent capability of 25 to 35 psia.)

In addition, as a result of these hypothetical accident analyses, safety features were added to plants to accommodate the hypothetical consequences of violent energy releases. These have taken the form of:

- Fuel elements designed to fail in a preferential manner
- Fuel debris catch pans - internal and external to the vessel
- Dispersal cones to provide a subcritical geometry
- Debris cooling systems
- Head hold-down systems
- Intermediate containment barriers
Missile shields

Auxiliary cooling systems

None of these safety features are expected to have to function since the accidents they protect against are never expected to occur, and they are simply added to the design to attempt to add extra margin to the system. One should add, of course, that none of these devices has indeed ever been called upon to act in any way.

The present position is not too different now from what it has been, despite the fact that energy release calculations have reduced so much.

Hypothetical safety devices are still added to plant designs in one way or another, but in a slightly different manner. Because the energy releases are small, safety devices have become correspondingly specific ... a debris catch pan designed to hold a part of the core rather than an entire core, vessel support systems designed to accommodate small movements, ... To go with this approach, present technologies have extensive development programs designed to characterize fuel failure and movement, and cooling of debris under the very severe conditions associated with these hypothetical accidents, in an attempt to make any safety devices so provided even more specific to certain failure modes.

There is a slight difference between approaches in different regions of the world. In Europe, a containment building is not always provided since it is considered that a low leakage mill-type building is adequate even for accidents which involve a release of fission products. In the USA, a containment building is provided from the outset of a conceptual plant design to provide a further barrier between fission products and the public. This is despite experience in accidents involving a release of plutonium in processing facilities which have indeed shown that mill-type buildings are quite adequate in preventing an escape of the isotope to the environment (5A and B). At the present time the public position in the USA, which is still in a much more adversary frame of mind than in Europe, makes these approaches consistent with the differing public attitudes. Further, in the USSR, since hypothetical accidents are not considered to the extent they are elsewhere, all external buildings covering reactors of this type are simple plate covered frame buildings (6).

However, it is recognized that Western European regulatory attitudes seem to have taken a more conservative swing lately as is evidenced by the comparatively high figures for Europe on Figure 1. Germany also considers it appropriate to use a strict containment shell building.

3. PREVIOUS EXPERIENCE

None of the above hypothetical safety devices or systems have in fact had to function in the way that their proposers assumed. Some, however, have contributed in a damaging way to the safety of the plant they were designed to protect.

The classical example of a contribution of an extra safety feature to an accident is the addition of a liner to the flow distribution cone in the inlet plenum of the FERMI reactor. The circumstances are well known; the liner came loose,
subsequently blocked the inlet nozzles to two assemblies thereby causing a meltdown of those particular assemblies. The flow distribution cone was in part a section of a meltdown redistribution and catcher system, and the addition of zirconium liner to this distribution cone was at least consistent with the design and fabrication of the debris catcher safeguard.\(^{(3)}\)

In the RAPSODIE system, although the original design had called for inerted cells below the operating floor throughout the plant, as a consistent protection against sodium fires, it was determined later that the disturbance to normal maintenance of the plant caused by this concept was unacceptable. Therefore, many of the regions below the operating floor were deinerted and only those cells immediately surrounding primary sodium systems were retained in an inerted condition. This change resulted in improved ease of maintenance and probably an improvement to the real safety of the plant.\(^{(3)}\)

In the FERMI reactor a decision in the design of the plant added a head catcher system which incorporated a small dome with an aluminum crushable missile shield which was designed to catch the head if any accident which caused the head jump occurred. This catcher system and the associated dome above the reactor head is very small and it confined the working space on the reactor head around the fuel handling machine and the control rod drive mechanism to a very considerable extent. Indeed, the confinement above the head is such, that although to my knowledge no accidents have occurred for this reason, it is probable that the real safety of the plant was diminished by the addition of this ultimate safeguard. In addition, the confinement of space probably did not allow adequate separation of the cabling to the scram and control systems, thereby making common mode failure of close proximity cabling more probable.

Additionally, one should also emphasize that much of active safety research is devoted to explaining and characterizing the consequences of extremely severe accidents, in particular those associated with the type of accidents used to assess the margin of containment. The phenomena chosen for investigation are those which have been predicted to possibly occur following the incredible initiators we have been talking about, and therefore, these have ranged from very severe explosion conditions in the past to, in the present era, also conditions of incipient fuel failure. In hindsight, because the present calculations are based on a mechanistic description of these incredible accidents, we would have to admit that the chemical explosion studies performed for pressure vessels and bulk containment systems have largely been wasted effort. The money expended on such effort would, in hindsight, have been far better applied to a study of lesser and more credible accidents.

It is not quite so obvious that the money expended in safety research today is not also equally inapplicable to the real safety of the plant. The TREAT program of Argonne National Laboratory is one of the most valuable safety research programs available in the world today to characterize fuel failure modes. Most of the transients are of interest in confirming design margins. However, although in no way are we dealing with the energy releases as predicted 5 to 10 years ago, nevertheless, we are attempting to characterize fuel failure also under very severe reactivity ramps which could only be associated with fuel failure modes outside of credible accident conditions. I believe, and it is a personal opinion, that those TREAT excursion tests performed for simulated $3/sec ramps are as unproductive to the safety program of LMFBR technology as were the explosive containment studies of a decade ago. Not only are they unproductive, but they may well detract our attention from the lesser consequence, but higher probability, safety problems.
Based on previous experience in existing liquid metal cooled fast reactors and based on trends in safety research, it would appear that the safety margin has not been increased by the addition of ultimate safety features, but in those cases which we know about has largely been decreased.

4. RECOMMENDED APPROACH

One of the problems with LMFBR containment evaluation to this date has been the need to evaluate each plant as a totally new and individual system since each plant has been a one-of-a-kind design. With the onset of the commercial program in which a large number of plants are likely to be built and evaluated by the year 2000, it would be prohibitively costly to evaluate each and every plant on the same basis as previously and it would not necessarily add to the safety margins of that plant. Therefore, a change in the present position on containment evaluation is proposed.

Table 3 indicates a recommended approach towards the incorporation of standard margins for safety in generic LMFBR's. In this present example, a loop type plant is considered, but the same approach can be also applied to pool type plants.

The table indicates that the present safety position for the LMFBR includes redundant shutdown systems, and the evaluation comprises the specific analysis of postulated accidents which go so far as to assume the total failure of these redundant shutdown systems, despite any degree of diversity, independence, and separation that those shutdown systems might have. As a result of this accident evaluation, safety margins are incorporated into the plant, either inherently or by design. Certain safety features, such as head holddown bolts, are also included because of this design basis evaluation.

It is considered desirable that in the commercial plant noted in the table at approximately the year 1980, being the onset of the commercial program, the containment evaluation should be standardized in an approved manner. An ideal situation would be the inclusion, not simply of redundant shutdown systems, but of diverse shutdown systems. The evaluation would not extend to specific accidents assuming the failure of those shutdown systems, but an evaluation of the inherent capability of the plant against unexpected mechanical and thermal loads. This evaluation is different from the present position in that it does not arbitrarily assume an accident with the failure of the shutdown systems of demonstrated reliability. I will return to this point later.

In addition to the evaluation of inherent capability, standard margins are added to the plant. These standard margins would be required for the heat transport system, in-vessel components, vessel and containment volumes, etc. These standard margins will also be described in what follows.

In order to bridge the gap between the present position and the commercial plant, several actions have to be taken and Table 3 indicates the expected course. In the very near future the Clinch River Breeder Reactor plant, now in design, will largely be the forerunner of the commercial program and therefore, would be expected to have a containment evaluation which lies midway between the present position and the desirable commercial plant position as shown on the table. Therefore, it is postulated that the conceptual design will also have redundant shutdown systems, will also include specific accident evaluations, and will include selected safety margins.
However, in parallel to the design of the Clinch River plant, three actions have to be taken in order to obtain a more realistic and more reliable containment evaluation. These are also shown on Table 3.

Firstly, considerable development must be undertaken, in order to obtain a reliable, and fast acting, diverse shutdown system. To date, such a system has not been designed, although a certain amount of work has been devoted to diverse drive systems and diverse locations of rodded systems. It is considered that a fast acting diverse, possibly fluid, system would be desirable, but development is required to overcome some of the problems of operation, maintainability and testing of such a system. In addition, this development program should have considerable attention applied to the testing of shutdown systems to prove their reliability in quantitative terms. This reliability should then be applied to a quantitative reliability analysis of the shutdown system. At this time, there is a considerable difference of opinion concerning shutdown reliability which lies at the root of differences between the regulatory position and the vendors. Ultimately, in view of the present trends, it will be necessary to carry out a probability and risk analysis of the plant, and the development of quantitative reliability figures for the shutdown systems would contribute to this assessment. (7)

In parallel with the development actions, it is necessary to be able to establish the philosophy and a licensing guide to provide for licensing acceptability of the new containment evaluation herein proposed. This action needs the combined assistance of the Atomic Energy Commission and of its regulatory branches and the nuclear industry, since such a philosophy could not be established through the medium of either the regulatory bodies alone, or through ACRS alone, or through the vendors and utilities alone. Such a radical change in containment philosophy from specific accident evaluations to standard margins requires the combined effectual assistance of all parties concerned in the development of a reliable safe LMFBR technology.

In parallel with these two foregoing actions, standards have to be derived. In providing a pressure-boundary report for reactor plant vessels, the ASME Boiler and Pressure Vessel Code Section III is followed in the evaluation of the structural capability of the boundaries of those vessels. This section of the ASME code provides standards which include allowances for the unknowns in material strengths and the unknowns in the loadings which could be applied to those boundaries. In other words, the code supplies standard margins for pressure vessel boundaries related to the design pressure, and the designer has no problem in applying these margins in a coherent and consistent way to this design. A parallel situation to this pressure boundary evaluation is desirable for the safety evaluation of the plant containment boundaries so that margins for unexpected accidents would apply not only to pressure boundaries, but also for in-vessel components, containment volumes and the like. This activity in providing such standards would include: (a) standard methods of determining the strength of various components, (b) standard loadings to be applied to particular components and the methods whereby these are derived, and (c) ultimately these standards would have to be written into a code by a code committee function. This code committee would comprise those people with adequate expertise in design engineering, in safety evaluation, and in structural evaluation to give industry guidance. Table 4 shows sample standards which could be applied to a design, based on available verified methods and based on the specific operating characteristics of a plant. The proposed code committee would need to make rulings on standard methods and values such as these and upon their application, testing and inspection.
In this way with the development of a reliable diverse shutdown system, the development of an acceptable licensing philosophy with which to apply standard margins, and the development of standard methods and margins it would seem that one could move towards a more logical and consistent evaluation of any LMFBR proposed for construction.

5. CONCLUSION

In summary, it has been shown that the specific evaluation of hypothetical accidents, and the specific provision of corresponding safety features has not in the past necessarily added to the safety of the plants so far designed, and in some cases has detracted from the real functional safety. This position has been engendered in past by a lack of agreement regarding the reliability of plant protective systems of non-diverse design.

It is proposed therefore that before the commercial phase of LMFBR technology arrives, a change of containment evaluation philosophy be entertained. The proposed position would include the application of "standard" margins to a plant design in many areas rather than a specific evaluation of arbitrary accidents.

In order to attain this goal, it is further proposed that a three part program is required; one in which a reliable diverse shutdown system is designed and tested and its reliability demonstrated; one in which a new licensing approach is developed by all interested parties in line with necessary standardization activities (12); and one in which standard methods and margins are technically determined.

In view of the difficulty of these tasks and the urgency defined by the short timescale involved, it is further proposed that the ANS or ASME take an immediate lead in the last of these activities through their standard functions. The AEC should seriously review this proposal and determine development funding priorities to allocate sufficient resources to an accomplishment of the first task - the development of a reliable diverse shutdown system, and the AEC should also take a prominent lead in establishing a real standardized evaluation philosophy.
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<td>BOR-60</td>
<td>USSR</td>
<td>1970</td>
<td>60</td>
<td>UO₂</td>
<td>176</td>
<td>2 loop</td>
</tr>
<tr>
<td>PARET* (1)</td>
<td>US</td>
<td>1972</td>
<td>50</td>
<td>Pu-UO₂ or C</td>
<td>260</td>
<td>1 loop + ACS</td>
</tr>
<tr>
<td>BN-350</td>
<td>USSR</td>
<td>1972</td>
<td>1000</td>
<td>Pu-UO₂</td>
<td>2050</td>
<td>6 loop</td>
</tr>
<tr>
<td>Phenix</td>
<td>France</td>
<td>1973</td>
<td>560</td>
<td>Pu-UO₂</td>
<td>3500</td>
<td>Pool</td>
</tr>
<tr>
<td>PFR</td>
<td>UK</td>
<td>1973</td>
<td>600</td>
<td>Pu-UO₂</td>
<td>6000</td>
<td>Pool</td>
</tr>
<tr>
<td>FFTF</td>
<td>US</td>
<td>1975</td>
<td>400</td>
<td>Pu-UO₂</td>
<td>3000</td>
<td>2 loop</td>
</tr>
<tr>
<td>JOYO</td>
<td>Japan</td>
<td>1975</td>
<td>100</td>
<td>Pu-UO₂</td>
<td>903</td>
<td>2 loop + ACS</td>
</tr>
<tr>
<td>BN-600</td>
<td>USSR</td>
<td>1975</td>
<td>1470</td>
<td>Pu-UO₂</td>
<td>3900</td>
<td>Pool</td>
</tr>
<tr>
<td>SNR</td>
<td>Ger. - Belg.</td>
<td>1975</td>
<td>736</td>
<td>Pu-UO₂</td>
<td>5000</td>
<td>3 loop + ACS</td>
</tr>
<tr>
<td>PRC (2)</td>
<td>Italy</td>
<td>1978</td>
<td>125</td>
<td>Pu-UO₂</td>
<td>?</td>
<td>2 loop semi-pool</td>
</tr>
<tr>
<td>MONJU</td>
<td>Japan</td>
<td>1979</td>
<td></td>
<td>Pu-UO₂</td>
<td>7900</td>
<td>3 loop</td>
</tr>
<tr>
<td>Clinch River Plant</td>
<td>US</td>
<td>1979</td>
<td>975</td>
<td>Pu-UO₂</td>
<td>7561</td>
<td>3 loop</td>
</tr>
</tbody>
</table>

* PARET Project was cancelled in 1965.
<table>
<thead>
<tr>
<th>REACTOR</th>
<th>NATION</th>
<th>CRITICALLY</th>
<th>CONTAINMENT</th>
<th>CONTAINMENT EVALUATION ACCIDENT</th>
<th>INITIATION</th>
<th>ENERGY RELEASE</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td></td>
<td>1. INNER 2. OUTER</td>
<td>EVENT</td>
<td>$1\text{ k/sec}$</td>
<td>TOTAL (MW secs)</td>
</tr>
<tr>
<td>EBR-I</td>
<td>US</td>
<td>1951</td>
<td>No Containment</td>
<td>None</td>
<td>-</td>
<td>-</td>
</tr>
<tr>
<td>BR-5</td>
<td>USSR</td>
<td>1958</td>
<td>Mill-type building</td>
<td>None</td>
<td>-</td>
<td>-</td>
</tr>
<tr>
<td>DFR</td>
<td>UK</td>
<td>1959</td>
<td>Steel shell</td>
<td>Core disruption</td>
<td>-</td>
<td>-</td>
</tr>
<tr>
<td>Enrico Fermi</td>
<td>US</td>
<td>1963</td>
<td>1. Concrete structure &amp; dome 2. Steel shell (32 psig)</td>
<td>1. Core disruption</td>
<td>Top of core falls on bottom</td>
<td>80</td>
</tr>
<tr>
<td>Rapsodie</td>
<td>France</td>
<td>1966</td>
<td>1. Concrete blast shield 2. Steel shell</td>
<td>1. Core disruption</td>
<td>-</td>
<td>-</td>
</tr>
<tr>
<td>SEFOR</td>
<td>US</td>
<td>1969</td>
<td>1. Reinforced concrete 2. Steel shell (30 psig)</td>
<td>1. Core disruption</td>
<td>-</td>
<td>-</td>
</tr>
<tr>
<td>BOR-60</td>
<td>USSR</td>
<td>1970</td>
<td>None</td>
<td>-</td>
<td>-</td>
<td>-</td>
</tr>
<tr>
<td>FARET*</td>
<td>US</td>
<td>1972</td>
<td>1. Reinforced concrete</td>
<td>-</td>
<td>-</td>
<td>-</td>
</tr>
<tr>
<td>BN-350</td>
<td>USSR</td>
<td>1972</td>
<td>1. Primary system 2. Mill-type building</td>
<td>None</td>
<td>-</td>
<td>-</td>
</tr>
<tr>
<td>Phenix</td>
<td>France</td>
<td>1973</td>
<td>1. Concrete blast shield 2. Steel shell</td>
<td>1. Core disruption</td>
<td>-</td>
<td>-</td>
</tr>
<tr>
<td>PFR</td>
<td>UK</td>
<td>1973</td>
<td>1. Primary tank 2. Containment building</td>
<td>Limited meltdown</td>
<td>-</td>
<td>-</td>
</tr>
<tr>
<td>PPTF</td>
<td>US</td>
<td>1975</td>
<td>1. Concrete structure 2. Steel shell</td>
<td>1. Core disruption</td>
<td>Sequential slumping of annular rings of fuel</td>
<td>50</td>
</tr>
<tr>
<td>JOYO</td>
<td>Japan</td>
<td>1975</td>
<td>Steel shell + concrete wall</td>
<td>-</td>
<td>-</td>
<td>-</td>
</tr>
<tr>
<td>BN-600</td>
<td>USSR</td>
<td>1975</td>
<td>1. Primary system 2. Mill type building</td>
<td>None</td>
<td>-</td>
<td>-</td>
</tr>
<tr>
<td>SNR</td>
<td>Germany/Belgium</td>
<td>1978</td>
<td>1. Reinforced concrete 2. Steel shell (doubled) (lower press, interspace)</td>
<td>1. Core disruption</td>
<td>-</td>
<td>-</td>
</tr>
<tr>
<td>PEC</td>
<td>Italy</td>
<td>1978</td>
<td>1. Reactor cavity concrete 2. Steel shell</td>
<td>1. Core disruption</td>
<td>Collapse of driver fuel following $1.5$/sec</td>
<td>25</td>
</tr>
<tr>
<td>MONJU</td>
<td>Japan</td>
<td>1979</td>
<td>-</td>
<td>-</td>
<td>-</td>
<td>-</td>
</tr>
<tr>
<td>Clinch River</td>
<td>US</td>
<td>1979</td>
<td>1. Structure and hot cell of concrete 2. Steel shell</td>
<td>1. Core disruption</td>
<td>-</td>
<td>-</td>
</tr>
</tbody>
</table>

* FARET Project cancelled in 1965.  
** Pre-Contract evaluation 1972
TABLE 3
SAFETY POSITION: LMFBR CONTAINMENT EVALUATION

1973
PRESENT POSITION
- REDUNDANT SHUT-DOWN
- SPECIFIC ACCIDENT
- EVALUATION
- DBA SAFETY FEATURES

DEVELOPMENT
- DIVERSE SHUTDOWN
- RELIABILITY TESTING
- QUANTITATIVE RELIABILITY ANALYSIS

LICENSED ACCEPTABILITY
- ESTABLISH PHILOSOPHY
- LICENSING GUIDE

STANDARDS
- METHODS
- LOADINGS
- MARGINS-CODE COMMITTEE (*)
  (ASME/ANS)

~ 1980
COMMERCIAL PLANT
- DIVERSE SHUT-DOWN
- EVALUATION OF INHERENT CAPABILITY
  "STANDARD" MARGINS (*)

(*) "STANDARD" MARGINS WOULD BE SET FOR HTS, IN-VESSEL COMPONENTS, VESSEL, CONTAINMENT VOLUMES, ETC. AS MARGINS ARE SET FOR THE PRESSURE BOUNDARY IN THE ASME B & PV CODE.

1656
Table 4
CONTAINMENT EVALUATION AGAINST HYPOTHETICAL ACCIDENTS - SAMPLE STANDARDS* (Loop-Type Plant)

(On assumption that specific core disruptive accidents are not considered, but margin is applied for energy releases of approximately 0.2 full power seconds)

1. METHODS

<table>
<thead>
<tr>
<th>Methods</th>
<th>Output</th>
</tr>
</thead>
<tbody>
<tr>
<td>Structural Evaluation</td>
<td></td>
</tr>
<tr>
<td>- In Vessel Components</td>
<td></td>
</tr>
<tr>
<td>- Vessel Walls</td>
<td></td>
</tr>
<tr>
<td>- Vessel Head</td>
<td></td>
</tr>
<tr>
<td>- Heat Transport System</td>
<td></td>
</tr>
<tr>
<td>Environmental Evaluation</td>
<td></td>
</tr>
<tr>
<td>- Containment Volumes</td>
<td></td>
</tr>
<tr>
<td>- Off-Site Dose</td>
<td></td>
</tr>
</tbody>
</table>

2. LOADINGS

<table>
<thead>
<tr>
<th>Loadings**</th>
<th>Allowables</th>
</tr>
</thead>
<tbody>
<tr>
<td>In-Vessel Components</td>
<td></td>
</tr>
<tr>
<td>Vessel Support System</td>
<td></td>
</tr>
<tr>
<td>Vessel Boundaries</td>
<td></td>
</tr>
<tr>
<td>Heat Transport System</td>
<td></td>
</tr>
<tr>
<td>Nozzles (outlet)</td>
<td></td>
</tr>
<tr>
<td>Hot Leg Components</td>
<td></td>
</tr>
<tr>
<td>INX/Secondary Boundary</td>
<td></td>
</tr>
<tr>
<td>Containment Volumes</td>
<td></td>
</tr>
</tbody>
</table>

* This list is intended only to be illustrative of types of standards discussed. It is in no way definitive or complete.

** Where loadings are presented numerically, they are still only illustrative of standards which might be set.
IMPROVEMENT OF UNDERSTANDING OF CONSEQUENCES OF HYPOTHETICAL ACCIDENTS

[Graph showing criticality dates and reactor developments from 1950 to 1980]

- USA
- Western Europe
- USSR

Enrico Fermi (1969) - PFR - PEC

Criticality Date


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REFERENCES


THE ROLE OF HYPOTHETICAL ACCIDENT ANALYSIS IN
FAST BREEDER DESIGN AND LICENSING*

Richard B. Nicholson
The Ohio State University

Abstract

The study of hypothetical core disassembly accidents is an essential process in the design and licensing of fast breeder reactors. The designer from the beginning of the design process must work toward a containment system that has minimum practicable failure probability for a core disassembly accident in the generic sense, with the specific initiating circumstances unspecified. In some cases this may mean increasing the natural containment capacity for a particular reactor concept several-fold, with associated reduction in failure probability of two orders of magnitude. Studies of specific accident histories are done to guide the designer but are not adequate to determine the bounding energy release on which containment system design is based. The role of engineering judgment in setting safety design criteria and the individuals making the judgments should be given greater visibility. This provides a force of individual responsibility to balance economic forces and it provides a basis for evaluation of the degree of competence associated with the judgments. The experimental program on fast reactor meltdown accidents must be pursued vigorously with new experimental facilities in order to reduce the extent of the judgment factor in the containment design basis.

*This work was done under USAEC contract No. AT(11-1)-2286. The opinions expressed are those of the author and are not endorsed by The Ohio State University or the United States Atomic Energy Commission.
For years there has been a large world-wide program in fast breeder reactor safety analysis. Two objectives of this work have been to generate the information needed to enable the reactor designers to design a safe reactor and to generate the information needed to establish to the satisfaction of the public and licensing authorities that the public safety requirements are being met. The activities to accomplish these two goals are not always the same, but do overlap considerably. In some cases activities that apparently relate to safety, when critically scrutinized, are found not significantly to contribute to either of the main objectives except through advancement of our general understanding of nature.

Over the past few years there has been an increasing body of opinion developing that the studies of hypothetical meltdown and disassembly accidents contribute nothing to the design of safe reactors and detract from the objective of licensing the fast breeder. A principal objective of this paper is to support the opposite thesis that hypothetical accident studies are not only useful but in fact an essential ingredient in both the design process and licensing. In particular, one design criterion must be the containment of all effects resulting from a hypothetical disassembly accident. I believe this statement to be true for any fast breeder that is built within at least the next fifteen years and perhaps longer. There is no way to avoid this design requirement in the foreseeable future. It is well within the capacity of the industry to design for containment, but not without the expenditure of considerable effort. It is essential that all designers accept this challenge and proceed to accommodate the necessary perturbations in the design process.

The safety analysts investigate a wide range of hypothetical accidents: a wide variety of types and wide range of severity. A very important segment of this work is concerned with insuring that the plant investment will be protected and that it will continue to operate steadily without long periods of shutdown due to malfunctions and equipment failures. This segment of the work deals with relatively probable kinds of malfunctions and how to prevent them or mitigate the consequences. Having accomplished this goal the designer has gone a long way toward insuring the safety of the public and successful licensing endeavors.

However, this part of the activity relating to economic assurance does not need to include studies of extremely improbable events involving multiple failures of normally reliable systems. If the probability of total loss of the plant investment is less than about one part in 1000 per year of operation, that is an acceptable economic risk. In contrast, it is necessary to insure at most one part in 10^8 per year that there will be no major release of activity to the environment. This paper relates to the question of public safety as opposed to the economic assurance. It is more restricted yet in dealing primarily with the hypothetical disassembly accident. It is not that the more-probable kinds of accidents are not at least as important. My concentration on the disassembly accident is because this is where the greatest controversy is beginning to surface. If this is not resolved soon it will build into an even greater problem area than emergency core cooling has been for the water reactors.

The LMFBR has one very important safety advantage over the water reactors. There need be no high pressure components within the reactor containment building. The double-ended failure of a primary sodium pipe is thus more unlikely relative to a water reactor pipe failure and secondly such a failure per se does not constitute a direct threat to containment. Because of the low pressure many of us believe the LMFBR may inherently be a safer reactor than a water reactor.
With no natural source of pressure to threaten containment, the public safety question for the LMFBR is mainly the question of whether there is any combination of malfunctions that can lead to a super-prompt-critical power excursion as a source of pressure. A lesser problem is the potential for sodium-air reactions and another one of considerable importance is ultimate containment of a molten core with or without the prompt-critical excursion.

There are three main potential initiators for prompt-critical excursions: control rod movement, sodium boiling, and fuel rearrangement. Sodium boiling in at least the central regions of an LMFBR produces positive reactivity and if it is sufficiently fast and coherent over the volume of the core can drive the reactor prompt critical. If a fraction of the oxide fuel of the fast reactor is compacted into a suitable configuration with density of say 5 gm/cc or higher it will be super-prompt-critical even without any significant neutronic coupling to the rest of the core. When one assumes this to happen he is usually thinking in terms of a meltdown accident. The meltdown could conceivably result from either an over-power transient due to control system malfunction or from a loss of cooling at normal power level. Control rod movement can contribute to prompt criticality only if the rods are moved out much more rapidly than design rates, for example pressurized ejection due to mechanical failures.

Now one may ask, why not design fail-safe and redundant control and cooling systems so such accidents can't happen? This is exactly what is done, but most of us realize that the term "can't happen" is too precise if taken literally. It should be replaced with the statement that such accidents have an acceptably low probability of occurrence. The Regulatory Staff of the USAEC in the ATWS report has stated at least for the water reactors that "it is not necessary, nor even possible, to design nuclear power plants, or any other man-made device or system, for all conceivable eventualities that are physically possible." This is approximately equivalent to the statement that accidents of sufficiently low probability need not necessarily be considered in designing protection and containment systems and other engineered safeguards. I agree with this philosophy.

Some, if not most, of the breeder designers believe that they can design a plant that will have so low a probability for a core disassembly accident that it is unnecessary to consider such an event in design of the containment structures, i.e., that it is a class 9 accident. I do not agree that this accident in all of its possible variations can be shown to be a class 9 accident. Substantial advances in the technology will be required before such accidents can be ruled out. It is not yet foreseeable when in the future this will be possible. It depends critically upon a vigorous research program with new experimental facilities that have not yet been authorized and are still barely conceived in any degree of detail.

There is a large body of competent opinion in agreement with my general point of view on this subject. There is not so much agreement on the appropriate measures to accommodate the problem.

How does one determine whether any particular accident need be considered in design of containment structures? There are two crucial questions.

1. Given that no special containment design measures are taken for the prompt critical accident, what is the probability that there will be such an accident of sufficient strength to fail the containment?

2. What is an acceptable level for this probability? Even more basic, who should decide what is acceptable?
Let's examine the second question first. Clearly the acceptable probability depends upon the potential for release of plutonium and fission products, which in turn depends upon the strength of the power excursion. The regulatory staff in ATWS concluded that the probability for accidents beyond the design basis must be less than $10^{-6}$ per year and that a suitable target probability was $10^{-7}$. Some will argue that this is not small enough yet. The ATWS report treats these probabilities as applicable to accidents which "lead to radiological consequences outside the plant boundary in excess of part 100 guidelines." It does not address the question of whether these probabilities are still suitable for accidents that are very much worse than the limits of 10 CFR part 100 and the report is not necessarily applicable to the LMFBR.

Who should decide this question of acceptable probability? It is a sociological question that should be decided by some appropriate representatives of the people, not exclusively by engineers. The decision is certainly related to the risk-cost-benefit balance. There should be significant input from the Environmental Protection Agency on this question. One can expect the ATWS figures to be challenged through the public hearing and judicial procedures. In the meantime the target figure of $10^{-7}$ per year seems appropriate for the breeder.

The problem is further compounded by the fact that a $10^{-7}$ probability is virtually not measurable, i.e., it is not possible to quantitatively monitor compliance with a chosen standard. This is a very different matter from the monitoring of low level releases during normal operation of a plant.

If such a specification cannot be monitored for compliance, is there then any point in establishing it? Yes, because it tells the designers what to aim for in design of plant protective systems and containment structures and also the nature of the supporting arguments that will be needed to license the plant. It provides a basis for intelligent communication between the safety analyst, the designer and the licensing agency.

Having established an acceptable probability for exceeding part 100 limits, the first question remains: What is the probability that there will be an accident severe enough to fail the containment? The designers are beginning to address this question in a semi-quantitative way through fault tree analysis. There are certain kinds of failures and malfunctions that can be assigned meaningful numerical probabilities based upon industrial experience. Bearing seizures in a pump; weld failures; electronic component failures in the control system. By considering every conceivable branch of the fault tree that can lead to an excursion, one can in-principle develop a network of series and parallel probabilities that can be combined to arrive at an overall probability for a prompt critical excursion of a given severity. However, while some of the elements of the fault tree can be assigned meaningful probabilities, many others at this point in time cannot.

Let's look briefly at the classical flow failure combined with failure to scram. These may sound like simple events but both involve multiple failures. There is usually an emergency power supply, a multiplicity of control rods and probably two relatively diverse types of control systems. The designers seem confident that they can provide the desired reliability down to $10^{-7}$ failure probability or lower if necessary. One can set up the fault tree and analyze the probabilities. There will be some very significant arguments about the numbers chosen for particular events on the fault tree but there is some chance that agreement could be reached on a conservative choice at each step. But there is likely to be objection at the more fundamental level of the drawing of the tree itself. Has the analyst really traced all the paths to the prompt critical accident? Or was a common mode failure missed? If we are really aiming for a $10^{-7}$ probability there is some chance that an obscure failure
mode has been forgotten.

Given that there is flow failure combined with failure to scram (very unlikely) an extensive core meltdown is assured. The prompt critical excursion is not assured by those two events. Many believe the excursion is unlikely to develop. If a low value can be assigned to this conditional probability, i.e., the probability of getting the excursion given the meltdown, then the requirements on reliability of control and flow are less stringent. For example, if one could show the conditional probability to be of the order $10^{-2}$, then the target for prevention of meltdown could be $10^{-5}$ instead of $10^{-7}$. Not only would this be easier for the designer, but also there would then be much less chance that a relevant mode of failure has been missed.

I have often heard inferences that the studies of meltdown accidents are meaningless because the designers can and must achieve the low probability goal by designing for prevention. The arguments I have just given are intended to show that this is not necessarily so and that there is potentially a high return on the efforts invested in analysis of reactor meltdown accidents. If we can show that the probability is very high that the reactor melts down safely the design requirements for the control and coolant flow systems can easily be met.

To demonstrate safe meltdown is a formidable task and the demonstration is very much dependent on the details of a particular design. Some of the analysts are arguing that their calculations combined with certain experiments show that meltdown will not lead to prompt critical reactivity and a severe power excursion. In my opinion such calculations are not yet reliable enough to accept for licensing purposes. Even if the calculations done now seem to show safe meltdown we must nevertheless still assume otherwise. Gradually we understand the problem better and increase our confidence, but we are still far from our goal. We will not likely reach it until better experimental facilities are provided to verify the analytical modeling of the phenomena. It is important that new facilities be built and that the meltdown accident studies be pursued vigorously in support of our breeder program.

There is another approach to protection against this kind of accident which ought to be vigorously pursued in parallel with the efforts to prevent the accident. This is to determine in some sense the upper bound on severity and design to contain the consequences. There is much discussion about the feasibility of identifying a set of accident conditions that can be agreed upon as a bounding accident. There is no bound in the sense that all the more severe accidents are prevented by the basic laws of nature. There is only in the sense that all the spectrum of more severe accidents has a low integrated probability. I discussed the goal of demonstrating that if a reactor melts down it will melt down safely with no power excursion. Designing for containment is a way of modifying the requirements of that goal so that it can be demonstrated more easily and with greater certainty. The goal is then to show that there is a low probability that meltdown will lead to a power excursion greater than the bounding design basis accident.

The design basis accident approach does play a role in present discussions between breeder designers and licensing authorities in this country. But the emphasis has been upon efforts to show that specific accidents rather than bounding accidents can be contained within structures that have been designed for other purposes without significant consideration for core disassembly accidents. Some have argued that to significantly upgrade containment capacity beyond that naturally present would be excessively costly, cause unacceptable interference with the normal functions of the plant and perhaps decrease overall plant safety while only improving its capacity for an accident that is in any case incredible. I remain hopeful that this is not true.
What is the nature of the hypothetical accident studies that the designer should do, or commission, to aid himself in making containment design decisions? The popular cases to study in recent years have been an over-power transient at steady flow, and loss of flow combined with failure to scram. These two cases can be regarded as representative of all the class of accidents that produce overheating of the core. The core will not overheat unless the flow is reduced or the power is increased. The nature and severity of the incident of course depends upon the details of the conditions that lead to the over-power transient or loss of flow, as well as the complex physical phenomena of transient boiling fuel-coolant interactions, fuel element failure, etc.

These are certainly two of the appropriate accidents to study. After selecting these for study the analyst still has many judgments to make. He must decide whether to do a demonstrably conservative analysis, a best estimate analysis, or some compromise between these limits. Both limits should be done along with a few intermediate cases. The reactor design is not necessarily determined by the worst case the analyst calculates, but the spectrum of calculations must be available for the responsible engineer to evaluate before he can make an intelligent design decision. Otherwise, the two examples become too specific. One is not attempting to protect only against specific accidents but rather all accidents that have a probability above a certain level. The examples chosen for analysis must in some sense cover the range of possible consequences from not only the specific initiators chosen, but also from all the possible initiators that have escaped the attention of the designer.

There are many decisions to be made in the course of the analysis, partly because in such complex phenomena apparently identical initiating conditions do not always yield even qualitatively similar results. In addition, our ability to analyze and make predictions about those situations that do follow a definite course to reproducible consequences is not yet sufficiently reliable.

In the end the safety analyst must be able to provide the designer with a spectrum of accident consequences and an associated probability distribution. The probabilities involved are not all well defined component failure probabilities but also involve more elusive concepts like the probability that one phenomenon model is the correct one relative to other possible models. Perhaps we are not ready to evaluate the probability distribution as a table of numbers but have to rely on less quantitative language. Whatever terms are used they must provide an adequate communication between the safety analyst and designer and be translated into design criteria. Certainly the trend is to attempt to bring quantitative probability analysis into such decision processes and this is desirable. Numbers assigned to such concepts as the probability of correctness of a model clearly must be involved in the analysis and require a great deal of subjective judgment. Therefore if quantitative numbers are assigned they are at best order of magnitude estimates.

There are some who object to the notion that one can cover the spectrum of accident consequences, from all possible accidents, including those that have escaped conception, by taking a few representative ones and varying the models and assumptions. For example, in a transient over-power accident one expects the fuel to fail first near the core exit, and this is usually believed to be the most favorable failure location. Does one cover some unknown accident possibility by also doing the analysis with fuel failure at the core center or is this a ridiculous academic exercise? In any particular instance of this type I cannot point to another real accident which gets covered by doing the hypothetical variation. Nevertheless, I firmly believe that if such variations are carried out throughout the analysis then there is a very good chance that the envelope of protection
based on this work will be extended over some real un-conceived accidents, real
in the sense of having non-negligible probability of occurrence. We are talking
here of low probability accidents that are not expected ever actually to occur
in the lifetime of a plant. I know of no other way to account for the un-
predictable accident in the design of containment structures. One of course
provides protection through the diverse control systems; but also needs some
basis for design of containment structures, which after all are there only
because they may someday have a job to do.

The structures one is concerned with designing relative to the hypo-
thesical disassembly accident are not so much the shell itself but rather
interior structures which must mitigate the violence in such a way that the
containment building is protected. The reactor vessel itself is certainly
a key part of the defense. A very strong reactor vessel with high capacity
for energy absorption through distortion is one of the possible design concepts.
The penetrations for control rods and fuel handling equipment clearly require
special attention. If special core retention structures are included to
guard against containment melt-through by the core debris these must be
designed to function following the design basis accident and have to withstand
any associated violence. The overall package of special design perturbations
to increase containment capacity is not a trivial one. But if the designer
is willing to add several millions of dollars to design and capital costs I
am confident he can increase the containment capacity significantly.

The selection of the containment design bases and the verification and
certification that the plant meets the licensing safety standards involve a
multiplicity of engineering judgments. This is right, fully justifiable,
and unavoidable. Major engineering achievements in many industries have
involved considerations of public safety that were evaluated with engineering
judgment. There is the difference that the potential destruction is much
greater in the nuclear reactor industry and this places a greater burden of
responsibility on those making the judgments, as well as on the licensing
authorities who must evaluate the judgments and in some cases make their own.
Because of the potential risks, the public through suitable representatives
certainly has the right to demand that these judgments be given visibility.

Something which in my opinion would advance the cause of safety more
than any other single step is a licensing regulation requiring the reactor
designer to document for public view every major design decision relating to
containment combined with an identification of the engineer or engineers who
accept the responsibility for that decision. There is often, although not
always, a balance to be struck between economics and safety. Visibility
of the decision process can provide the essential force to balance the
economic force and insure that a responsible decision is made.

Aside from the pressure effect associated with identifying the responsi-
ble engineer, this procedure also provides the possibility for the public
and licensing agencies to evaluate competence. If public safety is based
upon engineering judgment then it is appropriate for the public to inquire
into the competence of those individuals who make the decisions. Nothing
can hurt the credibility of the nuclear industry more than for the public to
find that it is impossible to learn who is responsible for the key decisions
on safety.

I have discussed three main options to demonstrate safety relative to
hypothetical meltdown and disassembly accidents. One is to design plant
protective systems to prevent the accident. Another is to prove that the
reactor melts down safely. The third is to design structures to contain
relatively severe power excursions. I have explained that the second two
work together to some extent. Most engineers can agree that any single
protective system cannot be relied upon to have a failure probability below $10^{-7}$ per year. To attain this low a probability at least two independent systems are required. They should not merely be independent in the sense of power supplies, sensors and control rods, but in the jargon of reactor safety they must also be diverse, which among other things means that they operate on different principles and have no common failure modes. They must also be capable of regular testing and this is a limitation that eliminates some of the concepts having demonstrable diversity. The proof that there are no common failure modes is really difficult.

Some of the designers argue that it is much more desirable to attain the $10^{-7}$ failure probability by employing two diverse control systems rather than one control system and one containment system. I could accept this if I could convince myself that the two control systems do have the required degree of independence and diversity. But it is easier and more reliable to demonstrate independence and diversity between a control system and a strong passive containment system. My view is that one should provide two control systems that are believed to be independent and diverse and also a strong containment system that is believed to have no failure modes in common with either control system. Then we could be really confident that the desired probability has been attained.

What are the design requirements on this strong passive containment system? The basic requirement is that it must withstand $x$ megawatt-seconds of expansive work by either a sodium-vapor explosion or fuel vapor expansion process with a specified pressure-volume expansion characteristic. Alternatively, a high pressure structure can be provided which restrains the expansion process. By withstand I mean that the design leak-rate of the containment building is not exceeded due to some violent effect and that the core debris does not threaten to melt through the containment. The major objection to this approach has been the difficulty in justifying a particular choice for the number $x$.

One can define the conditional probability that given a core meltdown the consequences will be $x$ megawatt-seconds of expansive work or more: this integral probability curve is a decreasing function of $x$. Most of us would agree that it is a very strongly decreasing function of $x$ although we do not yet have a way of explicitly evaluating it quantitatively. Suppose that a factor of two increase in the design value of $x$ cuts the probability of containment failure by a factor of ten. Then if the natural containment capacity of a particular plant can be increased through design by a factor of even four, one reduces the failure probability by a factor of one hundred. These numbers mean very little but it should be clear that there is the possibility of vastly reducing the potential hazard to the public.

It is essential that a breeder design project start with a containment design specification as a target value from the very beginning. I would suggest a number of the order of 1000 Mw-seconds for a demonstration-size plant. Then as the design progresses and detailed accident analysis progresses, this number can be adjusted toward the end goal of an optimized design parameter just as the other design parameters are adjusted. However, the economic value cannot be readily assigned to a certain containment capacity. It is a matter of judgment, which should be guided by the goal of achieving a containment failure probability that is as low as practicable much in the sense of holding environmental discharges to values as low as practicable. One should not stop trying to improve safety once he is satisfied that the failure probability is below the $10^{-6}$ to $10^{-7}$ range. I am not here recommending design for class 9 accidents, only providing margin for an accident whose class cannot be clearly demonstrated.
To summarize, I wish to stress five main points:

1. Hypothetical meltdown and disassembly accident studies are an essential part of the fast reactor design process.

2. One should design interior structures to attain minimum practicable containment failure following a relatively severe Hypothetical Core Disassembly Accident (HCDA).

3. One should design at least two independent diverse safety systems.

4. One should acknowledge the role of engineering judgment and create visibility of both the decision process and the engineers who accept the responsibility for the decisions.

5. It is essential that the reactor safety research program be pushed vigorously and that new experimental facilities be developed for the study of meltdown accidents.

References:

The question to which this panel addresses itself, safety design bases, may well be the fundamental one for fast breeder reactor development, transcending some of the questions which have recently received more discussion, such as breeding ratio, or the conceptual design details of the Demonstration Plant. It would be presumptuous of me to try to speak on this broad subject as representing an official Atomic Energy Commission position, nor do I wish to speak for the Division of Reactor Research and Development, but only as a deeply concerned professional who has been fortunate enough to be somewhere near the forefront of reactor development in this Country for almost a quarter of a century.

We might look to Safety Design Bases to perform four individual functions:

1. Facilitate understanding and communication between the designer and those charged with safety approval;
2. Serve as a set of values to facilitate design engineering;
3. Assist in developing better understanding by the general public of reactor safety matters; and
4. Serve to ease approval responsibility by safety officials.

There is a fifth function which is seldom considered in the development of Safety Design Bases but which, in my judgment, should be paramount, especially for reactor systems in early, developmental stages such as the LMFBR, as opposed to more mature systems such as the Light Water Reactors. That function is to serve to inspire and motivate research, development and design activities which will contribute to the ultimate safety of that reactor system in the future. In other words, speaking to the present circumstances, the design bases for safety for LMFBR's in the immediate future ought not to be chosen primarily on the basis of easing the licensing process for the FFTF and the Demonstration Plant, but most importantly, they should be chosen for their potential effect on the safety of the hundreds of LMFBR's on which we must depend during the next century. It is my view that this latter function of design bases has been often overlooked in the past and is in grave danger of being overlooked now in the LMFBR program.

Although it is inaccurate to paint the different approaches to safety design bases as black or white, there are three rather distinct results from the failure to consciously and deliberately establish design bases looking beyond the reactors presently being considered. The first is that, if one emphasizes the short range objectives for a single early reactor plant, he is likely, in his ignorance of all the facts in the short range, to over emphasize
acceptance of the largest conceivable or hypothetical accident, thus 
establishing public attitudes and setting precedents which are difficult to 
disestablish when knowledge is further advanced. This then results in 
diversion of resources from obtaining fundamental understandings of safety 
phenomena to carrying out design and development associated with special 
features whose only objective is to accept the very large, highly unlikely 
accident. This is evident in the Light Water Reactor system in which early 
choice of the double-ended pipe break as the Design Basis Accident led to the 
recent circumstances in which design and confirmation of the Emergency Core 
Cooling System has become the overwhelming issue in Light Water Reactor 
Safety. I suspect few knowledgeable people really believe that ECCS is, in 
fact, the most important consideration in LWR safety. Yet, we have all lent 
ourselves to concentration on that subject rather than on the more fundamental, 
and probably far more rewarding subjects from a total safety point of view, of 
the basic integrity of the primary boundary. My estimate is that, not 
counting industry efforts, about 15 times as much has been spent in research 
and development toward proving ECCS will work, as on research toward under-
standing reactor vessel and primary system integrity so as to reduce the need 
for the ECCS function. In this sense, the double-ended pipe break design 
basis, which lead to the emphasis on ECCS, and on which licensing and public 
debate has been primarily centered, may have been, to some degree, a red 
herring which has diverted attention and effort from such basic safety issues 
as reactor vessel and primary system integrity.

I am not arguing that Light Water Reactors should not have Emergency Core 
Cooling Systems, nor that the double-ended pipe break should not be a DBA for 
LWR's. What I am arguing is that one must be very careful in choosing design 
bases which lead to decisions to install such systems that "the baby is not 
thrown out with the bath water." It is a matter of emphasis, and knowledgeable, 
responsible officials and technical experts like those in this audience must 
accept the responsibility for ensuring that the emphasis is not misplaced or 
ill timed.

I remember an instance in the early days of nuclear power in the Navy when the 
Ship's Characteristic Board, a group of Senior Admirals, reacted against 
building a nuclear powered surface ship on the basis that the first ship would 
make the rest of the Navy obsolescent. A position of almost equal cogency is 
one which has been voiced with regard to double-ended pipe breaks in Light 
Water Reactors, that no matter what the material of construction or the design 
care in fabrication, a DBA for LWRs would still be the double-ended pipe 
break.

It is such apparent lack of discrimination in emphasis on the important issues 
of safety in the choice of design bases early in the development of a reactor 
system which can result in something less than maximum benefit to ultimate 
reactor safety.

The second debilitating consequence of premature or misplaced emphasis in 
selection of safety design bases is the diversion of safety development and 
research from some of the fundamental safety phenomena such as those being 
discussed in this Conference, fuel movement during an accident, fuel-coolant 
interactions, fuel failure propagation, and the behavior of plutonium 
aerosols. Instead, urgent effort will be required on the considerable 
research challenge of developing information needed to devise such systems as 
external core catchers. Furthermore, once we embark on such courses we will 
surely be put into the position, as we were in the case of ECCS, of proving 
that the system is, in fact, 100% effective. Once we have installed the 
protective device, the public will assume the probability of the accident is 
one, and its presumed failure will become the basis for our assessment of it.
We are about to make a similar error in the LMFBR system. Slowly but surely the emphasis and center of controversy in the LMFBR is turning from what seems to me to be the central safety issues to one which is far less fundamental, in my judgment, namely, external protection against core meltdown.

We should not mislead ourselves into believing that emphasis on less fundamental safety questions; that is, questions on which work will be less useful in terms of the absolute integrated risk, cannot be made without penalty to the more important questions.

Many make the argument that we can have our cake and eat it too; that the emphasis on protecting against the consequences of maximum, highly improbable accidents will not in any way reduce the will and enthusiasm in research and development programs to further understanding of basic safety phenomena. This is simply not realistic. There is a finite resource in safety development and engineering, and the resources, will and enthusiasm of the researcher are always limited. Reduced to absurdity, if a plant is engineered and built to fully contain, in every respect, a 10,000 MW/sec accident, then, who could justify that much effort should be expended, from a public safety point of view, on the kind of phenomenological research which dominates this meeting of the ANS? If the assumption is that the pipe will fail, in any case, why spend research effort to learn how to make it more reliable?

A third debilitating effect of early misplaced emphasis in choosing design bases on effects of large highly unlikely accidents is that it seriously reduces the motivation and enthusiasm of the designer for imaginative attempts to design so accidents will not occur.

Thus, wrong emphasis on Safety Design Bases in the developmental stage of a reactor system, such as the present stage of the LMFBR, will reduce the efforts to truly understand the safety phenomena and use the understanding to design for inherent reliability and safety. One of the panelists on the platform here today said in an earlier paper, "The authors believe that our present approach with respect to fast reactors may be self-defeating. For a well-designed fast reactor, no accident involving serious release of radioactive materials can result if the scram system functions properly; however, one can always assume the failure of even the most rigorously-designed, fabricated, and tested scram system. Given this situation, the designer is left with little or no incentive to develop and incorporate innovative ideas, such as backup scram systems, to improve safety."

This effect has been particularly evident in the FFTF case. We believed in the FFTF project that there were two fundamental safety questions towards which our design efforts should be bent. The first was to assure scram. To this end, we decided to install two redundant systems, diverse in logic, and we instigated extensive programs for testing the Control Rod Drive Mechanisms and Control Rod Drive Line under the worst practicable conditions. We felt the second fundamental objective was to maintain coolant circulation following an energetic accident. To that end, for what I believe is the first time, we designed the Closure Head, Reactor Vessel and their supports dynamically to accept the maximum practicable loadings. The Head is a 21-inch thick forging, the largest forging ever manufactured in this country and the maximum thickness which could be designed and built to Code requirements. It contains energy absorbing materials on its underside. It weighs more than 200 tons. We attached the Head not to the Vessel, but to the Support Ring, the largest ring forging ever made by 84-3 to 4-inch diameter high tensile bolts, dynamically tuned to provide stepped restraining forces to reduce the accelerations of the Head and limit leakage paths. We supported the Vessel on straps dynamically designed to stretch under explosive loadings, the whole reactor enclosure being designed to provide the maximum practicable protection.
against loss of function of the vessel system as a boundary for the coolant. The Heat Transport System was designed in conjunction with the Reactor Vessel to assure circulation after an accident without any assistance other than from gravity.

None of these steps taken to advance what we believed to be the two most fundamental aspects of safety has been of much interest to the safety community.

About a year ago, we invited a number of persons especially interested in fast reactor safety to the Combustion Engineering plant in Chattanooga to inspect the Head and Vessel in their final stages of construction. Combustion Engineering and Westinghouse set up a full day's presentation which described the design from an energy absorption point of view, and the significant effort which had been made in the manufacture of the Vessel towards improved quality assurance and welding - and other metallurgical control procedures. The response was embarrassing. We held a party and almost no one came.

Similarly, with regard to steps that have been taken to assure scram, there has been little interest from anywhere in the safety community. The accident receiving most attention for the FFTF assumes that not one of the nine control rods in either of the two scram systems moves an inch toward the center of reactivity. We are faced with the same safety attitudes as if the scram system had not improved since the axe to cut a rope in Stagg Field days. In other words, no matter what we did, or could do, in engineering and building the FFTF, could have avoided or mitigated the assumption of failure to scram and subsequent assumptions of failure to keep debris cool within the reactor vessel. To quote again from the same panelist, "An acceptable design basis accident must be one that produces a large damaging effect on the reactor facility, but not to the point where radioactive release has serious involvement of the general public."

In these circumstances, what motivation other than one's conscience is there to attempt to design and develop reactors with inherently safe features? It is my impression that, so far as its standing in the safety community is concerned, FFTF would be equally acceptable with no particular attention to reactor vessel support, head strength, or scram system reliability.

Several months ago, while at the Combustion Engineering plant in Chattanooga, just before the Reactor Vessel and Head were finally packed and shipped, I looked at those carefully designed and beautifully fabricated massive structures and said to myself, "What a fool you've been. Millions of dollars and hundreds of man-months in design and manufacturing effort have been expended to no avail."

No, I don't really believe that. I am still of the opinion that the great effort made to assure the integrity of the reactor enclosure of FFTF was sound and has contributed significantly to the safety of that plant and potentially to the safety of other LMFBR's. Nevertheless, so far as the attitude towards the safety of the plant, even among some of those charged with safety analysis within the FFTF project, this major effort made to ensure the integrity of reactor enclosure and the circulation of coolant in an energetic accident is passed by as of little significance.

What are the fundamental safety issues for LMFBR's toward which we should address our safety design bases and consequently our research and engineering efforts? In my judgment they are the following:

1. Assurance of scram. If that is assured almost no accident in an LMFBR is of very serious consequence.
3. Understanding of fuel motion during the initial phases of an accident.
4. The energetics of fuel-coolant interaction.
5. Fuel failure propagation.
6. The production and behavior of plutonium aerosols.

I suspect there is a measurable difference in overall safety between top fuel plena as in our U.S. reactor versus bottom plena as in Europe. The method of core restraint, as suggested by Pierre Zaleski's paper, is significant. The design of the fuel pin and subassembly from the point of view of directing and facilitating fuel motion in an accident, or assuring against criticality, is not yet understood. All of these deserve, I think, increased, intensive research effort.

We have proceeded in RRD over the past six years with a central objective with regard to safety; to thoroughly investigate and understand these basic physical phenomena so as to be able to design for the prevention and mitigation of accidents. It is heartening in listening to the papers presented in this meeting, to see that a very large percentage of them treat these questions, and that a very large percentage of those which do come from the safety development programs instigated and supported by RRD. I submit to you that had we had, during the early stages of the LMFBR safety program and in the FFTF up to this time, a conventional design basis accident approach, far less would have been accomplished on these subjects. In this connection I refer to a statement for which the Chairman of this Panel has received some recent notoriety. "If you want to pull a rabbit out of the hat, sometime earlier you must put the rabbit in the hat." That certainly applies to safety research and development. If we want to really understand reactor safety in the best scientific sense, we must make the effort early in the game to understand the base phenomena which are central to the safety issues.

What I am suggesting today is that a clear recognition be given to the fact that the breeder reactor systems are still in their developmental stages, and that Safety Design Bases used in this stage should be carefully and deliberately chosen to attack the fundamental safety issues in that reactor system, and not primarily to make the licensing of the first one or two plants easier or more palatable. That will require an act of courage and faith on the part of all those responsible for the choices. It probably makes the immediate explanation to the public more difficult. It probably requires a more comprehensive technical assessment on the part of officials charged with safety approval and a more thorough technical understanding of the entire plant on their part. It means giving recognition, encouragement, and value to the efforts of the designers to squarely face the potential safety hazards and design a plant which will reduce those hazards to a minimum. It means supporting and emphasizing the research which aims at understanding the basic physical phenomena of accidents.

And, finally, what I am suggesting means that all of us must, to some degree, give up the "security blanket" of "Since we can't prove absolutely that it won't happen, we must put in provisions as if it will, even when we can't prove absolutely those provisions will work."

I am convinced that the true safety of breeder reactors in the decades to come, when it appears mankind must depend on them in the hundreds for a large proportion of its essential energy needs, will be significantly furthered by courageous, farsighted actions now in choosing the Safety Design Bases to be used for the earliest LMFBR plants, not on the basis of the immediately perceived short term advantages, but rather on the basis of their long term effects on LMFBR safety research and engineering.
In order to discuss safety design bases for fast power reactors, we have to define our terms. Last June at the ANS summer meeting in Chicago I defined an acceptable design basis accident as one that was severe enough to do gross plant damage and to raise concerns, but not sufficient (after much analysis) to seriously affect the general public. Consistent with this definition then, one might define safety design bases as the series of postulated events guiding the plant design such that an acceptable design basis accident will result. The complementarity of these two definitions is useful to illustrate the point I was trying to make in Chicago; namely, if the reactor is designed or has inherent properties such that a given design basis accident does not produce the necessary result (almost, but not quite, affecting the general public) then one merely has to change the underlying accident assumption.

A well designed LMFBR which scrams at the initiation of a potentially serious accident (e.g. A Guillotine Pipe Break, A Reactivity Transient Loss of Pumping Power etc.) will have safety consequences which are relatively minor. Thus, in order to meet the requirements of an acceptable design basis accident as I have defined it, it is traditional in fast reactor safety analysis and licensing procedures to assume a failure to scram concurrent with some accident condition.

More generally, all reactors have a source of fission products. Since the aim of the game is to almost (but not quite) have the fission products come in contact with the general public during the design basis accident, competent and imaginative technical people can, with any reactor system, meet this goal in a safety analysis. As indicated above, I am proud to point out that we in the fast reactor community have not been deficient in meeting our obligations. (See "Catch 22".)

I would like to examine this situation from a somewhat different perspective. There are three interacting facets which are implicit but not necessarily explicit in safety discussions of various reactor types. First is reactor safety design which I will define as the design measures intended to provide an absence of hazard or danger, or at least to reduce the probability of hazard to a negligible level. I believe that all of us - reactor designers, technologists, regulators, etc., are sincerely striving for this goal. Unfortunately, safety like beauty, sometimes lies in the eyes of the beholder, since there is as yet no clear-cut way in which to define safety in an absolute sense. I will come back to this subject later.

*The reference to "Catch 22" is, I hope, clear from the above discussion; namely, we have evolved a system wherein, to demonstrate the safety of a reactor and have it licensed, we have to raise concerns that it may not be safe.
Second, is licensing, which is the formal process required in order to permit one to build and operate a reactor. Although safety and safety conditions are an inherent part of the licensing process, licensing also encompasses a series of formal and sometimes ritualistic proceedings not directly connected with safety. Finally, there is the public perception of reactor safety which, assuming the technical work is well performed, may be the most important aspect of the entire safety and licensing process, but which our formal processes fail to adequately address.

One has to realize that the general public knowledge of nuclear reactor technology and its ability to adequately assess safety is no better, and probably worse, than the ability of members of this audience to assess the relative hazards and benefits of a new surgical technique.

It appears to me that our whole approach to design bases and the licensing process ignores this and thereby foreordains a lingering fear on the part of the public no matter how well we do our technical work and no matter how well, as a result, we reduce any real hazard to negligible proportions.

Consider the building of a bridge. The practice in determining the design load for the bridge is to identify the heaviest vehicle presently in existence and performing a reasonable extrapolation to weights expected in the future. The design load is then established by assuming that the bridge is loaded bumper-to-bumper on each lane of the bridge with vehicles of this weight. Both the design basis vehicle and the loading mode are established by the American Society of Highway Engineers. If you or I asked about the safety of a bridge, we would be told that the design of the bridge had a safety factor of over 4, even assuming that the bridge was loaded bumper-to-bumper with the heaviest vehicles possible.

If the bridge were designed as a nuclear installation, we would load it bumper-to-bumper with the heaviest vehicles. Then we might assume that there was a gasoline truck in the middle, that the gasoline truck had been defectively loaded and that as a result the gasoline started to leak out when the truck was in the middle of the bridge. Not knowing the atmospheric conditions at the time, we would assume absolutely stagnant air, allow for the gasoline to vaporize so that the most effective mixture of gasoline fumes and air would be present, and then assume that counter to instructions the truck driver lit a cigarette. The analysis of ensuing events which, according to government regulations should assume that the entire bridge is at 1500°F might indicate that the bridge would still hold together and that the most highly stressed supporting member still had 10% margin to ultimate yield. We would then, upon inquiry, tell the public that there should be no concern about the safety of the bridge because even under very remote circumstances the bridge still had a 10% safety margin before it would fail. And lest my intent be misinterpreted, let me say that this method of presentation, which can be very misleading, is one which our industry helped to create and is not solely an AEC invention. I will spare you the rest of the scenario which has the attorney for the "Save the Ferryboat" League cross examining the civil engineers. However, lest my example seem too far fetched and unrealistic, let me ask if there is anybody in the audience who feels more assured about the safety of light water reactors now that the maximum clad temperature of a guillotine pipe break accident has been reduced from 2300°F to 2200°F using the methods of calibrations prescribed by the AEC?

Let me examine the problem from one additional viewpoint. All of the reactors built in the United States have a high integrity, low leakage containment of one form or another. Suppose during the design of a reactor it became possible to add an additional containment barrier at very little cost, or no cost, because this additional barrier had multiple uses? It would seem that if this situation prevailed it would be prudent and useful to add this extra containment. The problem now that we face is to explain what the basis of this
extra containment should be. Since presumably the original containment met all of the requirements for nuclear safety it becomes difficult to define a safety design basis for the extra containment unless one assumes that the original containment fails. In our present safety and licensing environment, I am fearful that the net effect in terms of public and perhaps even industry perception would not be added assurance of safety, but rather heightened concern about the integrity of any containment structure. I can already hear Tom Cochran's next speech: "...according to the AEC, there will be 70 grams of airborne plutonium, which after failure of the first containment..."

The above discussion is intended to convey my concern that our present approach to the safety and licensing question is in the long run doomed to failure and in fact may be counter productive in terms of providing the motivation to make our plants as safe as possible. The basic problem as I see it is that nuclear plants are very safe indeed. The probability of a serious accident is very low. Safety and licensing considerations are formulated within a framework of design basis accidents were intuitive assumptions are made on events of such low probability that human experience and intuition is patently not applicable and, as noted above, within this framework the aim is to make assumptions which always end up in a near catastrophic situation. A possible solution is to utilize the tools of probability and reliability theory which are available in order to quantify the situation so that rational discussions can be held and clear standards set on which to develop a sound safety design. Thus, along with most of my colleagues, I await the publication of the Rasmussen study with the hope that in the U.S. it will be a major step in this direction.

In the meantime, what about safety design bases for fast power reactors? The fast reactor has, in my mind, a significant favorable characteristic which we should recognize. Large fast reactors have a negative Doppler effect which slows down the time scale of potential accidents so that control rods can be inserted. If control rods are inserted, the safety implication of the ensuing events is minor. The effect of the doppler coefficient is not a theoretical concept but has been proven beyond doubt in the SEFOR experiment and supporting zero power and reactor experiments worldwide. Further, with scram, potential accidents and the following events can be followed in great detail theoretically, and experimentally if necessary, and one can obtain very high assurance of the accuracy of the predicted results.

On the other hand, if one assumes an accident without scram, the situation becomes a theoreticians dream. Material properties, core motion effects, fuel failure modes, sodium fuel interactions, postulated additional accident sequences such as half the core lifting up and then slamming down on the bottom half, etc., all become areas where speculation can lead to investigations of intense beauty and satisfaction. Having worked in the details of this field some time ago, I can personally attest to the satisfaction one derives in developing a new equation of state for uranium oxide at temperatures above 5,000°F (especially since one can't easily be proven wrong).

My concern is that I don't believe this activity leads very far. The conditions which exist under accident conditions with failure to scram are such that if one solves one problem he can assume an additional problem of at least as great challenge and concern. Further, no matter how many experiments are performed one can always postulate that the exact conditions which would lead to a more disastrous situation had not been properly mocked up so that assurance from experimental data is not apt to close off the concern.

Having philosophized in the preceding discussions, let me now describe how I would approach safety design bases for fast power reactors. I believe the major emphasis should be on the instrumentation and scram systems.
General Electric has advocated for the past ten years that there should be at least two fast acting scram systems in a fast reactor; maybe there should be three or more. I would devote major emphasis to the development of these systems and their associated instrumentation and the statistical testing of them both in-pile and out to assure that the probability of failure to scram is made negligible. And I would avoid like the plague any formal analysis which assumed failure of all scram systems and predicted in detail the ensuing events.

This does not mean that I would neglect core disruptions in designing a fast reactor. I believe that the theoretical and experimental work which has been done indicates that the probability of energy releases above a few hundred megawatt seconds is very small in any event. One can, with little additional effort and cost design fast reactor systems to accommodate energy releases of this magnitude and I would do this. However, what I would not do is try to mechanically follow a sequence of events which would try to justify this choice since as indicated above, I don't think this can be accomplished in detail with today's knowledge. And, finally, lest I be misunderstood, I would continue the intensive research and development efforts which try to explore the phenomena occurring in the hypothetical accident regime. I believe this information is and has been very useful in adding perspective to the overall assessment of the safety of fast reactors. However, I have little hope that on any reasonable time scale it will ever bound the problem in a way which will satisfy imaginative skeptics. In summary, I would put a containment around fast reactors whose design, in accordance with present practice, is determined primarily by sodium fire considerations. I would invest my resources as necessary in assuring that the reactor scavenged under all postulated conditions and I would trace in gory detail all off-normal events to assure that the core remained intact as long as there was a scram. I would, in addition, design the reactor and the containment system to accommodate an energy release of 100 MW seconds or so but would not try to trace in detail a sequence of events leading to this energy release. And, finally, I would not embark on the adventure of an external core catcher, since in my opinion this leads to another open avenue which will not be closed in the foreseeable future by either theoretical or experimental data. The external core catcher diverts attention from what I believe are the safety features of importance, i.e., those that keep the reactor system from entering a regime where events cannot be accurately determined, onto a device which I fear, even in the long run, cannot be shown to be reliable in the sense that it would bound an accident where other safety devices are assumed to fail.

EPILOGUE

Having indicated that I think it may be a fruitless endeavor to try and meaningfully describe in detail an accident without scram, I find like some of my colleagues, that this is an area of such fascination it is difficult to withdraw from it. Thus, let me make a few remarks.

The history of fast reactor safety has been dominated by two considerations. First, the neutron lifetime is so short that if there is a reactivity insertion greater than a dollar, events occur so quickly that no control system can be effective. This matter was of great concern prior to 1960 when Greebler at GE and Nicholson, then at APDA showed that large fast power reactors would have a substantial negative Doppler coefficient. The subsequent technical analyses and experimental work at SEFOR have shown conclusively that the Doppler coefficient slows the time scale to the point where normal fast acting control systems can safely shut down the reactor even under accident conditions involving initial reactivity insertions well in excess of a dollar.

The second concern is one of recriticality; that is, the possibility that under accident conditions without scram the core would come into a more dense
and reactive geometry. However, the characteristic of a fast reactor that reactivity increases with core compaction, also means that reactivity decreases with core expansion. It appears to the authors that the driving forces in a fast reactor core under accident conditions are such that one should expect expansion rather than compaction of the core. Indeed, there are data that would indicate that one might expect that this expansion would occur rapidly enough and with small enough damage that in a very real sense the fast reactor might be, or could be made to be, "inherently safe" even without scram. (See for example*) Thus, although I have expressed some pessimism at considerations of hypothetical accidents without scram, it appears that considerable satisfaction might be obtained if the avenue suggested were vigorously pursued.

CONTRIBUTION TO PANEL DISCUSSION ON SELECTION OF SAFETY DESIGN BASES FOR FAST POWER REACTORS

by

F R FARMER OBE BA FInstP
Safety & Reliability Directorate, UKAEA

Although this discussion is concerned with fast reactors, I would like to set a more general background which is concerned with the risk from any reactor system. My main argument could apply to the risks from any other industrial plant or social activities, but there is hardly time to develop this part of the argument during this meeting and I will remain on the subject of reactor safety.

In discussing reactor accidents, it is often convenient to refer to the iodine content of any release, although we know that other fission products have to be taken into account. A reactor of 1200 MW(e) will contain about $10^8$ curies of $^{131}$I. It is conventional practice to discuss a range of accidents to a level variously called the 'maximum credible' or the 'maximum design'. In the US you place accidents in one of nine categories of varying probability, of which the first eight include those accidents which are possible or unlikely, whereas the ninth includes those regarded as hypothetical. It is always shown that accidents analysed against the first eight categories lead to releases of radioactivity which meet the requirements for siting as required by licensing procedures and, as indicated in WASH-1250, most analyses show activity release perhaps one-tenth of the Release Guide Lines. This means that the release calculated is of the order of 100 curies $^{131}$I with some other associated radioisotopes representing only one part per million of the total fission product inventory of the reactor.

I have estimated that on a UK site - which is at least as populated as those used in the USA - that under average conditions a release of 1000 curies is unlikely to cause one case of thyroid cancer in the million or so people living between 1 to 50 miles - this is assuming a linear relationship between dose and effect. It follows, then, that from the point of view of risk to people rather than cost, the consequences of releases much less than $10^4$ curies are trivial although the cost in loss of output may be very high indeed and the social consequences may be considerable. The accident situations I am really concerned about are those in Category 9, ie those which could conceivably lead to a release of $^{131}$I and other isotopes from $10^5$ curies upwards.

Over the last few years there has been a growing recognition that we cannot absolutely preclude a major accident, but there should be reasonable arguments to show that the likelihood of such an
event is very low. In 1967 I suggested a figure of around $10^{-6}$ per reactor per year for a release of the order of $10^6$ curies and in Washington in 1972 I gave my own assessment of the possible consequences of releasing $5 \times 10^6$ curies of $\text{I}^{131}$-1 and other volatile or gaseous fission products. This indicated a probability of some 200 to 300 deaths over a period of 10 to 20 years following the accident, but that in a population of such a size, over the same period of time, some 10,000 people would die from accidental causes not related to the reactor.

Recent publications in the US, such as WASH-1270 and a recent Task Force Report to the Director of Regulation, indicate that similar objectives are being discussed in the US, for instance that the possibility of a large accident in excess of 10 CFR 100 should be less than one in one thousand years. This is later interpreted as less than one in one million per year per reactor which is exactly the same as my earlier proposal. The documents also indicate that the likely consequences of a severe accident would be not unlike large industrial accidents and in the range 100 to 1000 deaths.

In this introduction to my discussion I have indicated a trend which has developed over the last ten years and is leading to reasonably firm objectives in the future development of reactors. Obviously these objectives should also apply to fast reactors, ie we need to show that there is a similarly low risk rate of serious accidents and that their consequences are limited - not unlike those currently described as similar to industrial accidents. There would not be time in this panel discussion to prove or disprove the capability of the fast reactor to meet these standards. But those of us who have followed its development over the last twenty years have recognised many features which can add to its safety and which, if reasonably applied, could well make the fast reactor safer than current thermal systems. Although I should recognise good features in any system and praise them accordingly, it has been my responsibility to examine critically the weak features of any reactor and to encourage design and development to remove these.

We all recognise that the difficult part of an accident analysis for the fast reactor lies in assessing the consequences of a power excursion. This may be initiated from a minor event in a sub-assembly by the run-down of pumps or malfunction of control systems or other factors which might cause a compaction of the reactor core. Many types of accident have been studied and analysed by increasingly complex computer programs. Amongst this range of possible or hypothetical accidents it is not unusual to see a description of a power excursion caused by a reactivity change of $\$100$ per second. This implies the possible addition of a few dollars over a few tens of milliseconds which would seem to me a possibility which could never be entirely ruled out. It happens now that in the jargon of our trade such a power excursion is described in mathematical terms as follows:

The addition of $\$100$ per second to a typical core of a 1000 MW(e) reactor would lead to a core internal energy of 25-30 GJ developing a pressure at the core centre of 20 MN/m$^2$ and having a work capability on core expansion in the range 1-2 GJ

This event can be described in another way. A reactor containing
15-20 tons of fuel is assumed to be subject to a power excursion which would cause the fuel to increase in temperature at a rate of several hundred degrees centigrade per millisecond until the central fuel reaches a temperature in excess of 5000°C and a mean fuel temperature of around 4000°C. The pressures produced at the centre of the core are several thousand pounds per square inch which would seemingly accelerate fuel at $10^{-9} g$ to velocities in excess of 100 feet per second. The expanding core would have the capability of producing several thousand MWsecs of work, which may be said to be like 1000 lbs of TNT but having a rather different rate of pressure rise. The shock effects of the expanding core would be much less than the equivalent TNT but the momentum imparted incompressibly to sodium has considerable importance in the transient loading of surrounding structures.

The safety argument now advances on two fronts:

1) To show that the model is excessively pessimistic on several counts - I guess that ingenuity in developing other more severe accidents will keep pace with the benefits achieved through the refinement of the model.

2) To show a capability of engineering containment by extrapolating model tests and also a capability of removing decay heat and controlling a dispersed core.

If we are to follow this route, I suggest that even although the accident itself is unlikely, nevertheless:

1) Can we tolerate the failure of containment and associated provisions? Based on my assessment, the answer is positively no.

2) Is there a substantial margin in all the functions of containment as compared with the conditions derived from the accident models? It would appear at present as though there would be little margin available if we extrapolated the current accident model up to the size of a 1200 MW(e) reactor.

3) Can further containment be provided which can be effective if the first containment failed? This is a possible but difficult route to follow.

I propose there is a further alternative, namely to show that the accident model does not apply in its present form for good and fundamental reasons.

Let me run through the basic argument again. We all accept that reactivity accidents are possible. A step change of $\Delta \rho$ is compensated by a temperature increase in which the hottest fuel approaches 4000°C, so that reactivity changes of a very few dollars cannot be compensated by temperature but require a rearrangement of the core. The expansion of the core in most models is brought about by pressures generated through boiling fuel - some models introduce other factors such as the frictional drag of the outgoing coolant, but this is so dependent on the model proposed that I do not believe it has any general significance. If the core is to be moved only by boiling $UO_2$ we must inevitably accept the consequences of high temperatures and high
pressures exerted over a very short period of time, usually of a millisecond or so.

The alternative is to show that the modest rearrangement of the core necessary to cancel the reactivity change can be brought about by moderate pressures exerted at lower temperatures by fission products contained in the fuel, or indeed by any other mechanism or materials added for that specific purpose. This route is being followed actively in the UK and theoretical assessments based on fairly simple models have been published over the last two years. We believe these effects would reduce the damaging work capability of the accident by at least an order of magnitude, would lead to low central pressures and only partial core melting.

We need more evidence to substantiate these effects and this is the evidence which we are now seeking, but I submit this is a more likely route to success than that previously followed.
The CEA and EDF are at the present time designing a 1200 Mwe prototype LMFBR power station (SUPER PHENIX Project). The beginning of construction is planned for 1975, after the construction licensing procedure which is to start next July. The site, CREYS-MALVILLE, is located along the RHONE River, near the Italian and German borders.

Early in 1972, the Nuclear Safety Department of CEA, to which I belong, felt it necessary to define recommendations for safety criteria of SUPER-PHENIX in order to give safety design guidance to the constructors at the pre-design stage. The final form of a 40 pages typewritten document was presented officially at the end of 1972 after consultation with all the participants in the program and several editions were made. The safety guidance document was approved in February 1973 by the licensing authority under the title of: "Recommendations for Safety Criteria Applying to the 1200 Mwe SUPER-PHENIX Fast Power Station".

I shall base my presentation on this document, giving in the first part some general ideas about it, and in a second part examples of various criteria with technical solutions taken, or envisioned by the designers, to comply with these criteria. This, I hope, will give a fairly good account of present french positions about the safety design basis for large power LMFBR's.

PART I

First, the safety design basis are recommendations only. The reason why is obvious. The plant will be a prototype and the technological development of the liquid-metal cooled fast reactors is far from completely developed. Like all other aspects, safety can't be rigidly frozen at the present time. Besides that, we all know that FR safety studies, like all other kinds of reactor safety studies, are waiting for some very important conclusions to validate firm tendencies still not fully demonstrated. For all these reasons, the safety criteria might and will probably evolve in the future.
Secondly, these criteria have been written in terms of general principles, trying as much as possible not to pre-judge technical solutions which may be chosen at the designer's initiative. At the limit, the designer may even sometimes go toward other principles but in that case, they will have to justify it.

Thirdly, the criteria were established on the following basis:

a) Above all taking into account recent French knowledge of sodium fast reactors based on more than 20 years of experience, especially on RAPSODIE and PHENIX. PHENIX, in particular, has been a constant reference and results of its start up, power rise and full power operation gave confirmation of some major safety options like core monitoring instrumentation performance, natural circulation, emergency cooling, etc... I shall provide more detail on these points in second part of the presentation.

b) The criteria also rely to the international experience, especially from the UK, USA and Germany. In particular a direct transposition of USAEC LWR criteria was not possible, but some of them have been used or adapted.

c) As I said previously the station will be a prototype one, operated on a site situated in a low population density area. One must also note that the reactor will be of course a pool-type one and some of the recommendations are strictly linked with this concept.

In conclusion of this first part, here is the way the document is articulated:

There are 3 main chapters
- Chapter One deals with general design criteria
- Chapter Two is a list of the accidents which must be assessed in the safety analysis
- Chapter Three gives detailed design principles of protection against the accidents listed, in terms of methods for prevention, detection and action to cope with them or to limit their consequences if an accident reaches further stages.
PART II

To illustrate these general remarks I have chosen 7 points for which I will give both the main recommendations presented in the document, and ideas on the technical design provisions and engineered safety features that are envisioned to comply with these provisions. Here are the 7 points: protective system, primary system, core accidents prevention, whole core accident and containment, sodium fires protection, seismic design and external missiles protection.

1. Protective System

Important factors about design basis requirements for this system are:

- 2 systems are recommended, a main one and a backup one based on a different principle. This means that the backup system must be different from solid absorbers controlled from the upper reactor structures. The aim is really at the principle level to avoid the potential of common mode failure in the classic system that is entirely actuated from the control plug, and not because of prejudging of any kinds of defect in this classic system. The designers are working on different concepts at the present time and the most promising seems to be an articulated system of absorbers actuated just at the upper level of the core structure.

For the classic system, the following requirements are given:

- It must be divided into 2 completely independent systems, each one actuating half of the control rods. This includes geographic separation of all circuits, connections and power supplies.

- The reactor must be able to be scrambled in less than one second and maintained subcritical in the cold condition assuming simultaneous failure to trip of one system and of one rod of the other system.

- Design of the protective system and of core reactivity will be such that: with either the case of a single rod ejection, or of complete and uncontrolled withdrawal of one group of rods at maximum possible speed (one group being the maximum that can be actuated one at a time), then
- Trip of reactor must safely control the transient, or
- if the trip fails, technological limits on fuel pins will not be reached (that does not mean of course that the specified limits for normal operation won't be reached).

2. Primary System

Once again, this part refers to a pod-type concept for which the whole volume of the primary sodium, including auxiliary systems like sodium purification and B.C.D. systems, are entirely contained inside a single primary containment without any external pipes. (This was almost the case for PHENIX except for auxiliary systems; for SUPER-PHENIX everything is designed to be integrated).

The various required characteristics are divided in two main categories referring to the containment function of the primary system and to its cooling function.

a) In terms of containment function:
- constructive dispositions will be taken in order to make accidental or spurious draining out of primary system impossible,
- a safety vessel will be provided around main vessel, designed to allow in-service inspection of the main vessel and such that in case of a failure of the main vessel, the sodium level will be higher than the core and IHX inlet levels, thus allowing decay heat removal by natural circulation,
- the safety vessel, its upper closure structures, and structures around them must safely contain the consequences of whole-core accidents (we will come back later on this point).

b) In terms of cooling function:
- Natural circulation must be possible in the whole primary system (this is very important in our opinion. One of the advantages of the LMFBR system is its ability to insure emergency decay heat removal by simple and static means based on natural convection. Natural convection ability and performances have been successfully demonstrated on full scale at PHENIX recently).
- Decay heat removal system will be insured by several independent and redundant paths with, in case of complete loss of the normal system (including complete loss of power supply for forced convection), an emergency system completely independent. SUPER-PHENIX is being designed in this regard with:
- very large inertia systems on primary-pump power supply aiming, at the limit, at avoiding damage to fuel in case the pumps run down without a scram. This seems possible but is still under study.

- An emergency cooling system external to the primary containment, independent of any system connected with primary sodium forced convection and away from the influence of direct mechanical consequences of accidental core explosive accidents. It will consist of a water system, over-designed and separated in several independent loops, used in normal operation to cool the concrete around the primary containment. We believe that such a system, which is static, permanently tested and independent of primary system internals is a major safety characteristic.

- Besides, an internal core catcher is under design.

3. Core Accident Protection

Like we all know, fast reactor cores are very sensitive to cooling defects due to high power densities that develop in a compact geometry which is divided into small coolant channels. The cooling-defect type of accident must be prevented and detected not only because of the immediate consequences, but also because they might be theoretically, through a propagation process, at the origin of a whole core accident.

a) Prevention

Besides all classical but very important requirements for quality control of all components (fuel pins, subassemblies, core structures, etc.), here is a list of design requirements to prevent occurrence of cooling accidents at core level:

- Hydraulic holddown of subassembly (S/A)
- Mechanical systems to physically prevent S/A positioning in wrong flow positions
- S/A inlets designed to prevent complete flow blockages, with several large lateral feeding holes around a cylinder
- In order not to risk heating local blockages inside S/A, reactor operation with failed fuel that could release solid particulates of oxides into the sodium, must be prohibited. This means at the design level, that permanent individual delayed-neutron detection (NDN) must be done and leads to the necessity of providing a sodium sampling system for each S/A (such a system has been successfully operated and calibrated on PHENIX). This means also that we must know the characterization of different kinds of fuel failure because of course it would be too large a penalty to forbid any kind of operation with failed fuel. Consequently one must have the corresponding experience.

b) Core Monitoring System

Considerations on the nature of potential risks and on the importance of the problem to be solved, leads to the concept of a core monitoring system able to early detect any cooling disturbance and to trip the reactor prior to propagation towards an irreversible situation.

This system must be based, for obvious complementarity and redundancy reasons, on:

- Individual S/A detection
- Global core detection
- And correlation between the two types of signals.

For better understanding we will first describe the PHENIX system briefly and then talk of SUPER-PHENIX, for which substantial improvements are foreseen.

For PHENIX, the following surveillance is provided:

- At the global reactor level:
  - Reactivity monitoring
- Bulk sodium delayed neutron detection
- Gaseous fission products monitoring in argon cover
- Acoustic boiling detection (not presently connected to the scram system)
- At individual S/A level:
  - Two sheath-thermocouples per S/A connected to specific computers providing a permanent comparison between S/A ΔT's, thus allowing correlations at low thresholds and quick response time of the system
- DND from individual sodium sampling.

Practically our experience gives us confidence in the efficiency of such a system but we are aware also that some particular local blockage might, in theory, not be detected by the thermocouples, and might be detected too late by the other instrumentation if they propagate. That is why the following additions are foreseen for SUPER-PHENIX:

- Fast response time thermocouples at S/A outlets.
- Special computer treatment of all signals, working on correlation, which will trip the reactor on conjunction of very low variations on several parameters. That's mainly RAPSODIE experience and CABRI tests that give us the idea of such a system, the efficiency of it being based on the fact that the slightest local abnormality influences always more than one parameter; but only a computer may make, rapidly enough, the correlations at very low level.

There are of course special recommendations for these computers when being used to trip the reactor, for example:
- They must be entirely devoted to that single function
- They must meet reliability standards equivalent to those required for the protective system, for software as well as hardware.

One word on flow meters to end this question. We do believe that they might be very efficient to detect local blockages, but we have given up the idea to provide 2 or 3 of them at each S/A outlet. The complication would be too large and probably unsafe by itself.
4. Whole Core Accident and Containment

When the recommendations were written, the mechanistic analysis of possible whole core accidents were not undertaken and we stated at that time that the double potential risk of prompt nuclear excursion in case of large core melting, and of subsequent sodium-fuel interaction had to be taken into account for the dimensioning of the primary and secondary containment in a conservative way, even quite arbitrarily and regardless of the initial causes.

In continuity with PHENIX, the project has taken a working hypothesis in this regard consisting of a coherent gravity compaction of the core leading to a 60 $\text{s} / \text{s}$ ramp rate. The containment designed on this basis consists mainly in a primary containment made of main and safety vessels with a strong reactor roof, plus a metallic tight and resistant dome above the roof. The reason why this dome was designed is the difficulty to assess complete tightness of the roof when it is subjected to dynamic loads. In case of primary sodium leakage through singular points of the roof like the large components penetrations, the primary containment boundary is the dome which can sustain a complete spray fire of more than 1.1 tons of primary sodium (pressure = 3 bars). We see another great advantage to such a dome in terms of safety: it is to protect the roof which is a very sensitive part of the containment, against all kinds of external accidents like missiles and secondary sodium fires.

Mechanistic analysis of whole-core accidents are underway now. Partial results show that consequences for containment will be less severe than assumptions taken for dimensioning.

5. Sodium Fires

Let's say now a few words of sodium fires, which is a very important point in our opinion.

Primary and secondary Na fires have quite different characteristics:

- The primary fires are always radioactive and may happen under the form of spray fires which give the worst thermodynamic consequences (in terms of pressure and temperature for a given mass of sodium reacting
with air) if they result from an expulsion through roof penetrations following a core explosion. In that case however containment and limitations of masses of sodium that could be released in non-inerted parts of the reactor are relatively easy to achieve.

- The secondary sodium fires are inactive and are must more likely to be under the form of pool type fires, but the limitation of the masses that could react with air is quite difficult.

That's why recommendations are different owing to the type of fire:

- For primary fire prevention, because of their active character, it is required to always have at least 3 tight and resistant barriers between primary sodium and air, and to confine any active sodium circuits in inerted cells. Besides, it has already been mentioned that the limitation of air and primary sodium masses that could react in case of leakage through the reactor roof is achieved by means of a metallic dome above the roof.

- For secondary sodium fires, the general criteria are at 2 levels:
  
  - First, any possible secondary fire must not directly damage the primary system. That's why the dome also plays an important role in this regard because it provides a physical separation between sensitive parts of primary containment (the roof), and the rest of the installation. To complete this separation, leak jackets with inerted innerspace, around secondary sodium pipes inside the dome are also recommended.

  - Secondly, large secondary Na fires at definite sensitive points or components like pipes, pumps, valves, tanks, steam generators must not extend and cause severe damage to other sensitive parts of the plant. This requires a special whole arrangement of the plan consisting mainly of separation in cells (i.e. steam generators) or galeries (i.e. pipes), and devices to control the fire. I didn't say extinguish because it is not possible when faced with large quantities of burning sodium above 400 or 500 °C, which is the case in the secondary system. Large scale experiments done at CADARACHE for PHENIX have proved the efficiency of a device consisting of a bed of iron bars arranged in a special array, covered non-hermetically by two inclined steel plates forming a collector provided under sen-
sitive parts like pipes, tanks, etc, ... Such an arrangement allows collection of leaking sodium, reduction of air volume reacting, and reduction of temperature below 250 °C by a heat-sink effect. The result is a controlled reaction at low temperature leading to mild damage only on the immediate environment.

These systems will be used extensively for SUPER-PHENIX. Of course one must also mention the classical requirements for leaks and fire detection and alarm systems everywhere sodium is present.

6. Seismic Design

Seismic design is classical also and consists of taking into account a given design earthquake from pessimistic historical cases on the site. Let's say this earthquake is of degree "n" on the international macroscopic scale. Two criteria are given.

- For "n" seism, the plant must not suffer any damage that could lead to other repairs than minor ones.
- For "n + 1" seisms, the nuclear safety of the plant must be guaranteed: that is, the plant shut down, containment of dangerous products, and maintaining safe shut down conditions (in particular in terms of decay heat rejection).

For the SUPER-PHENIX site n = 7 (and of course n + 1 = 8).

7. Missiles

a) Internal missiles

Besides the general recommendation about protection against missiles internal to the plant, two particular points are stated with specific criteria:

- There must be an antismmle protection around the inertia wheels of primary pumps.
- The axis of the turbine must be perpendicular to the main axis of reactor and plant building, in order to be protected against rotor turbine rupture consequences.
b) External Missiles

A probability analysis of aircraft-crash risk showed that the SUPERPHENIX site is out of any airport landing or approach zone, and that consequently the accident to be taken into account was the crash of light planes up to 12,500 lbs, the probability of which being slightly lower than $10^{-6}$, extrapolated to the year 1990.

In that case the criteria states that primary containment must maintain integrity and that reactor shutdown and decay heat rejection must be safely achieved.

This led, in particular, to a reinforcement of the secondary containment which is of a circular type, with a concrete thickness of about one meter.

CONCLUSION

I hope what I have said is enough to show that we are developing a fast reactor system on a design basis consistent with safety. Let me insist, in conclusion, on some very positive inherent safety characteristics of LMFBER's which are not present in other types of reactors. I have in mind especially in this regard the permanence of core cooling, even in the absence of any power supply to insure forced convection, the physical impossibility of loss of primary coolant which is unpressurized and entirely contained in a static double vessel (at least with the pool type concept), the simplicity of core control because of its perfect stability, its safe dynamic response to reactivity transients and the large margins between normal operating condition for the fuel pins and their technological limits, due to sodium thermal characteristics. That's why in my opinion, designing safe LMFBER's in the present state of the art and of human knowledge and capabilities, without being an easy task, is a feasible one.
A REVIEW OF PANEL DISCUSSION — SESSION 16

by

M.E. Remley, R.T. Lancet, P.G. Lorenzini

Atomics International Division

Rockwell International Corporation

Discussion following the presentations both indicated some agreements and emphasized a number of controversial areas in which decided disagreements are evident. The fundamental question reviewed by the panelists is what should be the design basis of fast reactors to assure the safety of the public. It is axiomatic that the risk to the public should be low, and all panelists agreed that an acceptable low risk is presently attainable. However, the level of risk that is acceptable and the most effective approach to limit the risk is not at all clear.

There was a general consensus that all design approaches should assure positive termination of any nuclear chain reaction. This should be achieved by use of independent and diverse shutdown systems, and should include both diverse and independent trip signals, information channels, and neutron absorbers.

It was also agreed that provision for containment should be included in the design basis of fast reactors. However, there was no clear agreement on the magnitude of the energy release which the design should contain, or whether the containment should be based on a specific energy release at all.

According to one approach, some engineered safety features which are currently incorporated to protect against hypothetical accidents, in fact, compromise plant safety. The emphasis should be on reliable operation and control, rather than on "hypothetical safety." Under this approach, the fast reactor containment design would incorporate standard margins. Design safety would then be verified by establishing the reliability of systems and components.

An alternative approach was based on the position that this level of reliability cannot be assured, and therefore, the plant must be designed for specified hypothetical accidents. Under this view, if reactors were designed to contain hypothetical accidents, it would be easier to establish low risk levels for the public, since the level of reliability required for plant shutdown and other systems need not be as great.

A third approach recommends the study of hypothetical accidents, but with the analysis aimed at understanding the phenomena in the accidents, and using this understanding to form judgments and provide guidance for developing containment designs.
The panelists appeared to agree on the need to include provisions for containment of explosive energy releases and molten fuel. They also agreed that the major emphasis should be on reliability of systems and instrumentation, rather than on engineered safety features. They seemed to disagree on what standards should be applied in deciding the level of containment and the provisions necessary to assure molten fuel cooling following some hypothetical accident. They also disagreed on the emphasis that should be placed on hypothetical accidents. As one panelist pointed out, we are not free to perform unlimited analysis and testing in all areas, because we have limited resources to work with. Therefore, it is necessary to establish priorities in deciding how to allocate the resources.

This divergence in the approaches to the safety design basis for fast reactors affects the question of what is an acceptable risk, how should it be decided, and who should make the decision. While these questions are not all directly relevant to the safety design basis, they are an integral part of the area of public acceptance of nuclear reactors, and hence are a natural outgrowth of any discussion of safety designs and evaluations. Again, no clear consensus answers developed in the discussion. The nearest approach to consensus was that a major accident probability of $10^{-7}$ per year would be an acceptable one for fast power reactors, and it was generally believed that an accident rate at least this small would be attainable for commercial fast reactors. In the discussion, some took the view that public risk is not going to be defined quantitatively, in the mathematical sense; rather, the risk will evolve as a result of the comparison of the risk of obtaining electrical power from nuclear sources with the risks from other sources of power (e.g., fossil fuels). In particular, this comparison must include an overall "social risk," which involves not only cost of power and hazards from the various fuels, but also the costs of not having power, or the deleterious effect of prohibitively expensive power.

The question of how the decision should be made, and who should make it, presented a significant controversy, which ranges from the approach that, since this is a public risk, it should involve public debate and perhaps be decided by public vote, to the approach that the issue is a highly technical one, and hence should be decided by those sufficiently technically informed to make the judgments. There was universal agreement that the bases for all such decisions should be made completely available to the public.

In summary, the discussion appeared to reflect complete confidence that fast power reactors can be designed and built with satisfactory safety and acceptable risk to the public; however, the definition of acceptable risk has not yet evolved, in a generally agreed quantitative form, so that the criteria for evaluation of acceptable risk have also not yet evolved in an agreed format. Though there is a general consensus on some significant aspects of the approach to the safety design of the fast power reactors, the extent of the accident analysis and the applications of the results of the analysis to specific designs leave considerable license to the individual design groups.

It is becoming clear that diverse views exist on the approach which should be followed in establishing design bases for the safety of fast power reactors. The conference provided a forum for airing these views, and attempting to understand the fundamental issues which are at stake. The discussions provided the opportunity to air views, define philosophies, and begin to crystallize the issues which must ultimately be resolved.
AFTER DINNER PANEL:
ACCEPTABLE LEVELS OF RISK TO THE PUBLIC--AND WHY
"Acceptable Public Risk and Why"

by

Thomas B. Cochran

Natural Resources Defense Council

Presented at

American Nuclear Society

Fast Reactor Safety Meeting

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To borrow a phrase of now Vice President Gerald R. Ford, acceptable public risk is whatever the public perceives it to be at the moment. Different people perceive risks in different ways. What is acceptable to one person is not acceptable to another. The examples are all too numerous -- smoking, floridation, flying, safety of nuclear reactors, skydiving, Nixon vs. McGovern, or simply Nixon as President of the United States. I add the last to clearly illustrate that the time at which the assessment is made is important. Public perception can be strongly influenced by information, be it fact, opinion, false statement, or dogma -- political or religious.

Some risks identified as public are assumed by most, but not all, without choice, some genetic diseases and "acts of God" for example. Others have been placed on society historically, through technological development. These risks in many cases also have been assumed for one reason, or another, without public choice. In some cases, the choice was made in the early stage of development when future risks were not well known. The
development of the automobile arguably falls in that category. It surely does as presently perceived by at least a minority.

All people are to some degree risk averse. Life insurance is a manifestation of risk aversion. It is noteworthy that the cost of term life insurance is necessarily higher than the value of the policy weighted by the actuarial probabilities of life expectancy. Some risks associated particularly with technological developments do not manifest themselves until future generations. In other words, there can be an intergenerational transfer of risks relative to the benefits, and vice versa. Here, the risk-benefit tradeoff is perceived by some as an ethical, or moral issue that cannot be resolved except by public choice. Faustian bargains fall into this category.

Many public risks can be and many are assumed by choice -- often by government. The decision to accept a risk can be made by edict, fiat, democracy or personal choice for example. Some risks are generally perceived to be so minimal as they do not constitute matters of government. It is difficult, however, to think of a major technological development that did not or does not involve governmental decision making, in part, because government outlays account for most of the R&D expenditures in our country. Some technologies are so pervasive that decisions related to development of these and their attendant public risks should be made with public choice. The electric power industry is a good example here. Not only is the use of electricity pervasive, but its generation can also pollute the air and water -- both public goods.
In the United States it is a widely held view, and a tenet of our constitution, that the appropriate mechanism for public decision making is by representative government -- a democratic process. Governmental decisions often result in laws. These can increase or decrease public risk. One would not expect, nor does one find uniformity in public risks which are "acceptable" in the eyes of the law. It is legal to drive a car at 30 mph through a residential neighborhood or a crowded city street, however, it is illegal to drive at any speed past a school bus discharging children.

In his book, *The Closing Circle*, Barry Commoner rightly observes that the critical issues posed by advanced technology "are matters of morality, of social and political judgment." "In a democracy," he notes, "they belong not in the hands of 'experts,' but in the hands of the people and their elected representatives." Since the fundamental choices are value choices, a governmental response which effectively delegates the control of technological developments to a panel or group of technical or scientific experts is unacceptable. Because of this delegation of responsibility, a government decision to accept a risk does not imply that it is acceptable to the public. Furthermore, there is extensive literature analysing the effect of coalitions and government structure on decision making. In the best of all worlds representative government does not imply or reflect uniform representation.
Public participation in each phase or aspect of a federal effort to assess and control technologies is essential to the success of that effort. The greater the information and the more diverse the points of view to which government is exposed, the more accurate its appraisal and predictions are likely to be. Geesaman and Abrahamson have written in the *Bulletin of the Atomic Scientist* (Mar. 1973, p. 18):

> The wise use of technology, insofar as it is attainable in a democratic society, will better derive from decisions based on diverse pluralistic inputs and open adversarial confrontations, rather than on the unilateral assessments or judgments of monolithic institutions. In the social evaluation of technologies, pluralistic controversy complements rather than contradicts scientific objectivity.

One can in theory define a median level of acceptable public risk as the level of risk that 50 percent of a population finds acceptable. This however cannot serve as an operational definition and has no useful purpose where public risk decisions are made by representative government.

In conclusion, when in the course of development of a technology that is pervasive and has risks that are perceived by segments of society as unacceptable, and when you find yourself debating the question of an acceptable level of risk for this technology, the question should be resolved by public choice. In the United States the appropriate mechanism is through representative government. This should be an informed decision with the fullest public participation -- in other words, a decision reached following a national debate of the issue. In this debate different people will perceive the risks in different ways.
ACCEPTABLE LEVELS OF RISK TO THE PUBLIC - AND WHY

by

F R FARMER OBE BA FInst P
Safety & Reliability Directorate, UKAEA

My work has mostly been concerned with nuclear reactors and associated chemical plant, their design, and latterly their associated risks of failure. In the course of this work particularly in reliability assessment I have had some association with other industries particularly the chemical industry. In fact the first ten years of my career were concerned with chemical engineering before the nuclear power programme started in the UK. Whereas I am aware of some of the risks to health from other industries, I am not an expert in that field and will develop the theme of this talk on only a few examples with which I am reasonably familiar.

The widespread use of methane, propane, butane and other petroleum products has led to at least 100 accidents being reported over 40 years. Over 200 people were killed in one accident in 1944, the damage from which was estimated at several million dollars. There were 50 events between 1950/1964, averaging $<10^6$ per year, and generally less than ten casualties.

In the last ten years the cost has increased to $20 \times 10^6$ per year, although with few casualties. This cost increase is said to be due to the larger size of operations and other expensive plant in association with these installations. What do I conclude from this? That progressively there will be some improvement in the standard of design and operations, but there will also be an increase in the number of installations.

There will be accidents in the future - some will kill people, most probably in the decade 1/10; there will be some risk which may relate to the 1 in 10 accident in a decade 10/100, and a small risk, perhaps 1 in 100 with casualties of 100 to 1000. In my view this latter range has a probability of between $10^{-1}$ and $10^{-2}$ per year. Would we accept this for the nuclear industry? Not intentionally. There are several differences in the way in which conventional industrial accidents are viewed as compared with nuclear, such as:

1. Many industrial accidents have occurred - people are used to them - and they always happen to somebody else.

2. Our society cannot manage without natural gas and petroleum products and feel that the advantage we gain from their use outweighs the disadvantage accruing from accidents and the financial loss involved.
(3) The accidents are of a type, which having once occurred, the consequence is known and limited.

The situation which we imagine to arise after a nuclear accident would be somewhat different. Most casualties would not occur immediately. There would be a greater fear of becoming a casualty than the probable risk of so becoming.

Various measures would be taken after the accident, such as milk and/or food restriction or diversion, some movement of people, some restriction on access to ground or other facilities. It is likely that the public assessment of the degree of harm arising from the event would, in fact, be far greater than that which would ultimately come to pass.

There is a further difference between the current position of nuclear power and our use of petroleum products which would be argued by some people today - that we could do without nuclear power or have it in some other place, or in some other form - this may not be the assessment in another 20-30 years' time.

Having in mind these differences, it is felt by a number of people that the risk of a serious event which could lead to some 100-1000 casualties should be smaller than that which seems to occur in other industries and recent papers in the US seem to align with the opinion I gave in 1967 that we should aim at reducing the risk of such an event to less than $10^{-3}$ per year or less than one million per reactor per year.

At your Annual ANS meeting in 1972 I gave a brief description of what I thought to be the consequences of a major accident to a thermal reactor on an average site. I said then that if an accident occurred it could lead to a 30 per cent probability that 100-1000 cases of thyroid cancer would develop over the next 20-30 years or so, or a 20 per cent chance that the number could lie between 1000-10000 and I thought this could lead to some 200-300 deaths, i.e., this is in the range of some large industrial accidents. If we could design and operate reactors to make the risk of this event one per million per year, then there would only be about one in one hundred chance that this event could occur this century. Indeed, it would not be altogether unreasonable to accept a slightly higher risk rate of $10^{-4}$ per reactor per year, although the acceptability of the event does depend, as I said earlier, on whether the event happens shortly or after another twenty years or so.

In proposing these, or any other targets, we imply a belief in the randomness of those defects in plant, in imperfections in operation and/or maintenance which might together cause a minor event to become a major accident.

This we also believe and act upon when told that the likelihood of serious accidents to aircraft is about $10^{-6}$ per flight. When an accident occurs it is difficult in the case of aircraft, and will be difficult for reactors to determine whether the accident was one of a random set or due to a weakness to which all members might be prone. This uncertainty is serious in the early life of aircraft or reactors, and matters less in the later life of the set - but this is of little help to the nuclear business when similar reactors cover a time spread of 30-50 years.
I wish now to pose a problem of a different type. We are aware of accidents in industry and to aircraft which might kill 100-1000 people. If we could foresee that developing an industry, transport, or other activity, could give more severe consequences by one or two orders of magnitude - would it be reasonable to proceed if the likelihood of the event were 10 to 100 times more remote?

This is not entirely hypothetical. The possibility of a jumbo jet crash on a football stadium has been assessed by various authors at, I believe, a risk rate of $10^{-8}$ per year. I can see the reasonableness of this assessment and appreciate the random factors which contribute to this low number. We see no direct association between the major defect on the aircraft, the presence of a football field, and the timing of a match.

By this example, I imply the need for a considerable degree of independence between several factors which might increase the severity of an accident by one order of magnitude, as might arise through the combination of wind and weather pattern which, for airborne hazards, can lead to at least two orders of difference in total harm to people.

I repeat the question - do we believe that there is an upper limit to the consequences which society might suffer - for worthwhile benefit - irrespective of risk rate, or do we accept the logic of associating increase in severity with decrease in anticipated frequency? For my part, I accept the logic of the latter proposition but might in practice find it very difficult to believe that a frequency of $10^{-7}$, $10^{-8}$ per event or per year can be established with reasonable conviction unless significant contributions to this low number arose from clearly random causes or causes having a high degree of independence one from another.

So far I have touched on two types of hurt to people, one having immediate effect - explosions, fire etc - and the second having a delayed effect of 10-20 years, which, having been initiated is largely irreversible, as through inhalation or ingestion of radioactive materials, carcinogens etc. There is a third area of interest as might occur through the ground deposition of radioactive material, or conceivably long-lived dangerous chemicals.

Emergency reference levels have been propounded which seem to be chosen at a risk level of between $10^{-3}$ and $10^{-4}$ per person which, in the case of persistent whole body radiation, is assessed as the likelihood of delayed leukaemia or cancers if continuously exposed to the higher radiation background. If the numbers of people are small, action can be taken to decontaminate or move people, and I guess that most of us who are not affected will not view the event too seriously.

However, if the number is large and the risk rate conceivably in the range $10^{-2}$ to $10^{-3}$ what action is possible? Does this imply the setting of a high degree of improbability to this event? Will society respond equally to harm already committed as to harm yet to be committed, or what cost or effort might be demanded to reduce the accumulation of risk already present but not fully received? I have no answer - and maybe the answer would be very different in different countries at different times.
ACCEPTABLE LEVELS OF NUCLEAR RISK AND WHY

Sidney Moglewer - Sierra Club

The issues involved in the assessment of nuclear risk are complex and strike at the very heart of our social fabric. The Sierra Club has long been concerned that an acceptable level of risk be achieved. Unfortunately, our present institutional mechanisms and political management leave much to be desired. It is the Sierra Club contention that the basic problem is not one of potential technological solution but, rather, a cultural/political/social breakdown in the management of this new technology.

What we have is a new phenomenon in our history—the very low probability of a very high and long-lived catastrophe. Normal expected value decision theory breaks down in this instance as a useful tool. It is audacious for any group of technocrats, the AEC or the nuclear industry or the university community, to specify an "acceptable" level of risk. "Acceptable" to whom? What we have here is a political decision by the citizens of this country as to the degree of risk they will tolerate. This is vital if our democracy is to survive.

At the present time, there is no formal mechanism for the public to express their views except by costly, time-consuming judicial litigation and testimony before legislative bodies. The public is often handicapped by a lack of objective data and analysis, and is also subjected to a barrage of propaganda from the industry and advocacy studies from a self-perpetuating bureaucracy. No wonder they are suspicious and they have every right to be where their health and welfare, as well as those of future generations are at stake.

In order for our citizenry to make rational decisions on nuclear risk, we need a means for the technical community to provide them with a spectrum of alternatives and consequences. Obviously, zero risk will cost an infinite number of dollars. But how about the rest of the cost-risk schedule? Don't give us one point on the curve and ask us to blindly accept it.

The Atomic Energy Commission is attempting to allay public suspicion by the sponsorship of the Rasmussen report on nuclear safety. In public speeches officials of the AEC, including Chairman Ray, have portrayed this major study as proving the safety of nuclear reactors. Rasmussen himself has testified before the Joint Committee on Atomic Energy that this study will provide major guidance for AEC decision-making. Mind you, the Rasmussen study will not be out until June and will certainly not have had a critical review by that time. If, in fact, the Rasmussen study were to be used in major decision-making, the AEC would suspend major decisions until after a thorough review of the report. I have not observed this. The Sierra Club strongly suspects this study will only produce another advocacy document. However, recognizing its importance, we are trying to assemble an independent, objective review team. It is one of the tragedies of our time that there is no formal mechanism to insure independent, objective reviews of these critical studies. Funding for such review efforts is extremely difficult to obtain. Unfortunately, practically all studies are only adequately reviewed by their sponsors. This is not a healthy situation for our society.

I do not know what level of nuclear risk the public would judge acceptable. There is no extra-governmental mechanism for risk assessment. There is a lack of a statistical basis for confidence in statements of the AEC and the nuclear industry. There has not to date been an open and healthy dialogue between the
involved groups in this country, though that situation is rapidly changing for the better. There has been political mismanagement of the program characterized by inadequate and unbalanced research and development programs and premature commitment to OMB dictates, guided primarily by a penurious instinct and not adequately sensitive to problems of the environment and nuclear risk. But the public is becoming aware of these problems and this spells profound changes for the future.

The Sierra Club is suspicious that the present planned deployment of 1,000 reactors by the year 2000 is premature. Reluctantly, we in the Club are moving in the direction of the nuclear moratorium—we are extremely aware of the environmental hazards of fossil fuel plants—until technology can demonstrate what the public will consider an acceptable risk—and we develop the institutional mechanism to properly implement this necessary public decision. We will continue to vigorously support the requirement for major research and development on all types of reactors and processes. And we recognize that we are moving into a new age of high technology and sophisticated management. Can our social institutions keep pace?
The topic of today's panel reminds us of similar headlines we have seen at various occasions in recent years. When I got the invitation from Dr. Okrent to contribute to the panel, I asked myself, whether there might be any new aspect except the renewed statement that the level of nuclear risks accepted by the public is still very low, and that the situation is not improving neither as far as I can see in the United States nor in my own country. On the contrary, efforts like those of Ralph Nader in the USA, and what we call "initiatives of citizens" (Burgerinitiativen) are gaining momentum.

What are the difficulties? Apparently the problem is not to describe any specific failure of a nuclear plant. In principle it is possible to evaluate its consequences to the outside in a straightforward way. More difficult is the question, whether we are able to foresee all possible failures. Here I think we have to admit that at least the public might have the impression that there exist residual modes of failure that we might not be able to foresee and therefore not be able to describe and evaluate with respect to their risks for the environment.

Much more difficult apparently is the evaluation and classification of events and their risks with respect to their frequency of occurrence. Well known in this connection are the British efforts to establish a probabilistic approach. You all know the work of F. R. Farmer, who tried to classify the risks and their acceptability by relating the frequency of occurrence of a failure versus its consequences expressed by the whole-body dose in man - rem received by a properly defined average individual. This approach suffers from a deficiency that we had several times the opportunity to discuss with Dr. Farmer, namely that it is nearly impossible to collect all the data and parameters necessary to evaluate adequately the frequency of occurrence as well as the consequences - quite aside of the fact, that there is a certain ambiguity in the well known Farmer curve that yields a definition of acceptability and that culminates in the well known catastrophic accident with the frequency $10^{-9}$, the so called "class 9 accident".

This already has brought us to the center of our topic, namely the acceptability of risk. Here we remember the well known work of Chauncey Starr on the public acceptance of mortality risk, arising from involuntary exposure to socio-technological systems (such as motor vehicle transportation). He was able to show that our society has accepted a range of risk exposures as a normal aspect of life. He has also shown that the nuclear plants so far have caused lower death-rates than e.g., ordinary power plants of the same level. Consequently, Starr states that any risk created by a new socio-technological system is acceptably safe enough, if the resulting risk level is below the curve that he has derived from observation of actual facts.
Starr has also indicated that one had to distinguish between voluntary and involuntary risks and that the public is ready to accept voluntary risks, e.g., in the public traffic, that are much higher than the involuntary ones. (We come back to this point later.)

The question remains, therefore, why the public risk acceptance level for nuclear plants is so much lower than usually anticipated by its promoters, or expressed otherwise, why there is insufficient public nuclear risk acceptance. And why is this so in spite of the fact that benefit, necessity and to a certain degree also the advantage for the environment in normal operation are so obvious to the expert and well known and even partly acknowledged by the public.

The problem seems to be much deeper than it appears in the discussion up to this time.

The people in Karlsruhe who deal with the problem are inclined to call it a question of communication. Of communication

- from the promoters to the public and its individuals

as well as

- from the public and its individuals to the promoters.

In the communication from the promoters to the public three difficulties seem to exist:

1. The problem of communicating scientific and technical knowledge;
2. The problem of transparency of the process of decision and safeguarding;
3. The problem of convincing the public of the necessity and of there being no alternative.

The three items point to deficiencies on the part of the promoters. Obviously it will always remain nearly impossible to communicate a full scientific and technical knowledge of any complicated process. The backside of this situation is that the public nevertheless becomes aware of the fact that also on the side of the promoters apparently open questions are admitted. But the problem is deeper. Nuclear energy appears to the public as being promoted not only by vendors or utilities, but also by irresistible automatisms of facts, inherent to our technical society, to our socio-economical system, that is kept moving ahead by a host of different specialists. The difficulty for the individual to understand what is produced by science and technology, makes him progressively sceptical, especially if there are dangers involved that one tries to discuss away. A crisis of loyalty appears on the horizon. The only solution seems to be that the public, the individual concerned, gets the feeling that he can participate in the process of decision, at least by being convinced that the processes of decision and surveyance or safeguarding are transparent and have a structure that guarantees the responsibility with respect to the society. The problems turn out to be a political one and the solution can be found only in the political regions.

To improve the public acceptance for nuclear energy, it would be urgently necessary to evaluate and to present seriously possible alternatives of primary energy production and to prove that in spite of many statements to the contrary, there is no alternative to nuclear energy in the foreseeable future. Presently the public realizes that many of the statements concerning the
alternatives are of opportunistic character and have not enough background. It is absolutely necessary that it becomes obvious that in all such questions non lobbyists are fighting and struggling for the possible and the tolerable.

I also mentioned that there is a problem of communication from the public to the nuclear promoters. We have already stated that science indeed is able, at least in principle and to a certain degree, to evaluate the consequences of technological developments, but that science is not able so far to offer an approach to the question, whether these consequences also are accepted. The question, whether a risk is sufficiently low, cannot be left only to the judgment of experts, but in a democratic society is a matter of the individuals concerned. How and in what way an individual can be put into a position to decide in such a question, is a different, a political problem.

Part of the public concern against the nuclear risks also has its origin in the fact that one knows that in the deterministic analysis of risks potential failure mechanisms are known and will be prevented by re-design. The degree of analysis is unknown. Furtheron, the public has a feeling that in the probabilistic approach one already admits that a full re-design doesn't make sense or even is not possible. A certain residual risk remains by definition. Apparently the question, whether a residual risk is acceptable to the public, cannot be answered by science only. The answers could come only from socio-psychological studies.

One has also to admit that even the direct methods of information exchange from the individual to the nuclear promoter so far is insufficient. A direct discussion between the public and the promoters on a pure technical and factual basis at least in Germany is in a very early beginning.

Let me finish these remarks with a few criteria about the readiness of the public to accept a certain risk that partly have also been stated by Chauncey Starr. Risk is accepted, when the following criteria are fulfilled:

1. Immediate benefit (example: using a car in public traffic)
2. Personal influence possible,
3. Analysis of risk is transparent,
4. Conviction that one is not forced to enter the risk,
5. Personal satisfaction,
6. One has become accustomed,
7. One is ready to accept a relatively high risk, if level and benefit are known.

The level of risk seems to be unacceptable, if the following conditions prevail:

1. The process of decision is not transparent,
2. The level of risk cannot be influenced,
3. The proximity of the benefits is too small (selfishness),
4. The behaviour of the promoters causes suspicion,
5. Little empirical knowledge and missing quantitative risk analysis,
6. Not enough own knowledge,

7. The risk cannot be shut down,

8. There is an incalculable residual risk the consequences of which could be large and the true probability of which is unknown.

Apparently we can all see that much work on the acceptability of nuclear risks is ahead of us.

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LOS ANGELES CHAPTER - AMERICAN NUCLEAR SOCIETY
NUCLEAR UNDERSTANDING SEMINAR
1974 FAST REACTOR SAFETY MEETING

THE BEVERLY HILTON HOTEL (MONTE CARLO ROOM)

WEDNESDAY, APRIL 3, 1974

PROGRAM

8:30 a.m. REGISTRATION (LOBBY)

9:00 a.m. CITIZENS' WORK SHOP ON ENERGY AND ENVIRONMENT featuring the United States Energy-
Environment Simulator

MR. PHILLIP KEARNEY, COLORADO STATE UNIVERSITY
MR. MIKE LOWENSTEIN, ADAMS STATE COLLEGE

Following an introduction, the audience will make "on the spot" decisions as they attempt to
supply the demand for energy from various sources, while they conserve valuable resources.

10:30 a.m. THIS ATOMIC WORLD
RAY PEREZ, UCLA, will demonstrate nuclear radiation and reactor safety involving audience
participation. Mr. Perez has been presenting similar educational material to high school
audiences in a program arranged by Oak Ridge Associated Universities.

11:45 a.m. BUFFET LUNCH (GRAND BALLROOM) (Free to Registrants)

12:30 p.m. THE KEY TO BEING A MORE EFFECTIVE PERSON
VERNE KALLEGIAN, PhD, Psychology (VERSAILES ROOM)

Mr. Kallegian, a well-known psychologist, is active in labor-management arbitration and
believes the key to successful relationships is mutually agreeing on expectations of each
other and providing appropriate feedback. Audience participation will demonstrate this
and related principles.

2:00 p.m. THE LIQUID METAL FAST BREEDER REACTOR AND ITS PLACE IN ENERGY SUPPLY
MR. RALPH BALENT, VICE PRESIDENT, LMFBR PROGRAMS,
ATOMICS INTERNATIONAL DIVISION, ROCKWELL INTERNATIONAL

The unique energy-supply characteristic of making its own fuel from the waste of today's
reactors, and the future of the LMFBR as an energy source, will be explained by this
internationally known engineer.

2:30 p.m. DISCUSSION

3:00 p.m. THE ENVIRONMENTAL IMPACT OF THE LMFBR AND ITS ASSOCIATED TRANSPORTATION
AND SERVICES
MR. ALAN A. JARRETT, CONSULTANT

A recent USAEC study analyzing the environmental impact of the LMFBR will be reviewed.

3:30 p.m. DISCUSSION

4:00-4:15 p.m. GENERAL DISCUSSION AND AUDIENCE SUGGESTIONS

1712
The
Liquid Metal Fast Breeder Reactor
(LMFBR)
and
Its Place in Energy Supply

by

RALPH BALENT*

at

Nuclear Understanding Seminar
of the
American Nuclear Society

Los Angeles, California
April 3, 1974

*Vice President, LMFBR Programs
The **Liquid Metal Fast Breeder Plant (LMFBR)** and its place in Energy Supply

In 1954, President Eisenhower announced the "Atoms for Peace" program, aimed at developing atomic energy for peaceful purposes. As a result of this program, the world today has the benefit of a variety of peaceful applications of atomic energy in the fields of medicine, industry, agriculture, research, and the production of electric power.

Nuclear power is projected to take over an increasing role in the generation of electric power in the U.S. The rate of growth of nuclear power as well as the optimum approach varies among the various groups who are attempting to solve the "energy problem" or "crisis" as it has been called since the Arab oil embargo of last year.

The inescapable conclusion is that nuclear power via the fission process is going to have to play a significant role if the U.S. is going to even come close to the goal of "energy self-sufficiency" within the next decade or two. This conclusion takes into account reasonable projections of the standard of living, industrial growth, concern for the total environment, and due regard to conservation of our natural resources.

In 1905, when Einstein announced the energy-mass equivalence, there were less than 100 million people in the United States and 1000 megawatts of electrical generating capacity served their needs. Today, there are 250 million people using 400,000 megawatts of power. With only 2-1/2 times the population, we are using 400 times more power. Even with modest expansions in population, the U.S. power requirements for the year 2000 (26 years from now) are conservatively estimated to exceed 2,000,000 Mwe generating capacity. This means that in the next 26 years we are going to build five plants for every one in operation today in the U.S.

In the U.S. today there are 43 nuclear central station plants in operation, and approximately 160 plants are either under construction or firmly planned. By 1980,
the cumulative nuclear generating capacity in the U.S. will be over 100,000 Mwe. All of these plants are thermal reactors and burn the uranium isotope U-235. Natural uranium contains only seven-tenths of one percent U-235, which is the only material Mother Nature gave us that is fissionable by thermal neutrons (see Figure 2).

![Figure 2. One Hundred Pounds of Natural Uranium](image)

The other 99% of uranium is made up of the isotope U-238 which by neutron absorption is converted to the man-made element plutonium which like U-235 is fissionable (Figure 3).

![Figure 3. Thermal Reactor Neutron Balance](image)

The current design of thermal reactors requires the ratio of U-235 to U-238 to be increased from the 0.7% to 2 to 3%. This increase (enriching) is accomplished in 'diffusion' plants. High grade uranium ore as mined contains approximately 1 part uranium to 500 parts rock. This ore has to be milled and refined to obtain U3O8 — called yellowcake. This yellowcake is transported to 1 of 3 diffusion plants in the U.S. where it is converted to a gas (uranium hexafluoride), the enriching (increasing the ratio of U-235 to U-238) is accomplished, and the uranium converted to UO2 (uranium dioxide). This material is shaped into pellets which are encased in tubes and fabricated into fuel assemblies for use in thermal reactors.

![Figure 4. Fast Reactor Neutron Balance](image)

While in the reactor core the U-235 fissions produced heat which is converted to electricity and at the same time some of the U-238 is converted to plutonium. Current thermal reactors produce 70 atoms of plutonium for each 100 atoms of U-235 they burn. Thus, they utilize approximately 2% of the energy in natural uranium.

Under development in the U.S. is a new kind of reactor which operates with fast neutrons and burns plutonium. These reactors are capable of producing approximately 130 atoms of plutonium for every 100 they burn (see Figure 4).
Because they use fast neutrons, produce more fuel than they burn and use liquid metal coolant, they are called Liquid Metal Fast Breeder Reactors, or LMFBR's.

Since plutonium is a man-made element the fast breeder reactors need an initial source of fuel to get started. It is planned that the plutonium produced in thermal reactors will be used as initial cores of LMFBR's after which the reactor will not only produce enough fuel to recharge itself but will produce excess plutonium which can be used to start up additional LMFBR's. After the initial charge of fuel, LMFBR's do not require operation of diffusion plants and only need a small supply of U-238 which can be in the form of wastes (tailings) from diffusion plant operation or natural uranium. Figure 5 shows the fuel cycle for both thermal reactors and fast breeder reactors.

Figure 5. Nuclear Fuel Cycles

What are the benefits of this new breeder reactors? Since LMFBR's produce more fuel than they burn, the only fuel costs associated with these plants will be the cost of reprocessing the fuel assemblies. Thus, LMFBR's have the potential of generating power at a lower cost than either fossil plants or thermal reactor plants. It has been estimated by the Edison Electric Institute and the U.S. Atomic Energy Commission that the savings in power costs from LMFBR's to the U.S. public and U.S. industry will be 50 to 500 billion dollars in the first 50 years following their introduction. In addition, there is currently sufficient uranium on hand to meet the energy needs of the nation for a hundred years, utilizing LMFBR's as the primary source of electrical power, without mining any additional uranium ore. Contrasting this with the coal requirement of 10 million tons mined per day, the FBR's "zero" mining requirement for 100 years certainly has an environmental, conservationist appeal.

Both thermal reactors and LMFBR's operate without any requirement for oxygen (i.e., air). The U.S. Navy recognized early this attractive feature of atomic energy and, under Admiral Rickover, developed the nuclear power submarine which has the capability of extending submerged propulsion by orders of magnitude, greater distances, and lengths of time than conventional diesel submarines. Since reactor plants can operate without air there is no problem of air pollution. It is true that thermal plants operate at lower temperatures than conventional, modern day fossil plants so that they produce more waste heat than conventional plants. However, LMFBR's will operate at high temperature and will produce less waste heat than modern day fossil plants.

What of the mushroom cloud? Reactors are not engineered like bombs; thus, no accidental sequence of events can result in energy release anywhere near that "engineered" into a bomb. Reactor manufacturers are required to study every conceivable and many hypothetical sequences of malfunctions which could result in possible accidents. Under an extremely thorough and systematic review and licensing procedure, reactor manufacturers are required to incorporate redundant and
diverse safeguards to preclude any possible accidents. In addition to the engineered safeguards, the reactor manufacturers are required to prove with extensive engineering safety factors that, even with complete failure of all normal control features as well as the simultaneous failure of the redundant and diverse safeguard systems, that even very low probability severe accidents will be contained within the reactor structure and containment building. That means that there will be no energy or fission product release from the site boundary which will result in any significant effect on private property or the general population. To date, the safety record of the U.S. nuclear industry, operating under licensing and compliance procedures of the U.S. AEC, has been perfect.

But what about small amounts of radioactivity which leak from a reactor even without an accident? Mankind has always lived in a sea of radiation from natural radioactivity in the earth and from radiation received from space. The average exposure from natural radiation sources to each individual in the U.S. is 100 mrem/yr (see Figure 6).

Since the atmosphere acts as a partial shield against radiation from space the difference in altitude between Denver and Los Angeles is such that an individual at the higher altitude receives a little more natural radiation (approximately 25 mrem/yr) than people living at lower elevations.

Current reactors are designed to limit exposure at a plant site boundary to 1 to 5 mrem/yr; i.e., approximately 1 to 5% of the average exposure from natural sources. Thus, a person who lived his entire life at the site boundary of a nuclear power plant would receive less increase in radiation exposure than if he were to live in Denver, Colorado, where natural radioactive exposure is higher than in Los Angeles which is near sea level.

Even though it has been shown that too much radiation can be harmful and massive radiation doses can cause death, the small differences due to altitude differences are not detectable and thus, 1 to 5% increase in natural background for a person who lived all his life at the site boundary of a nuclear reactor would also not be significant. Even so, the U.S. AEC and the nuclear reactor manufacturers are continuing to develop improved systems to reduce the small amounts of radiation leakage to a level approaching zero.

What about "all that plutonium" and a potential accident or sabotage during transportation for reprocessing or refabrication of the fuel and the long time storage and confinement of radioactive wastes? It is true that plutonium along with certain fission products are radioactive and as such the AEC has established strict standards which set very low levels for the maximum allowable amount of each of these substances in the natural environment. The nuclear industry recognizes these standards and designs all processes, procedures, and equipment for the handling, transportation, and storage to assure that under all conceivable accidents that these standards will not be exceeded. One of the advantages of nuclear power is the small amount of...
material and waste that has to be managed.

Even though no one can guarantee against sabotage the problem associated with providing adequate protection is not any greater than that which the world is facing and solving today in other areas.

With respect to storage of radioactive wastes, there are a number of ways which this can be accomplished and the AEC is studying the various options. The amount of material is sufficiently small such that all high level radioactive material can be stored in engineered facilities and monitored and guarded for many years; i.e., centuries, until a suitable permanent disposal scheme is accepted.

Westinghouse is Lead Reactor Manufacturer and Atomics International and General Electric are major subcontractors which are teamed together to design the U.S. first large LMFBR demonstration plant. The plant is to be constructed on a site in Tennessee on the Clinch River. The Clinch River Breeder Reactor Project is jointly funded by the U.S. utility industry and the U.S. AEC and is planned for operation in the early 1980's.

Additional LMFBR plants will follow such that commercial LMFBR's will eventually take over as the principal nuclear generating plant in the U.S. It is estimated that by the year 2000, the U.S. generating capacity will be 1/2 fossil (coal, oil, and gas) and 1/2 nuclear. Of the nuclear central station power plants, 1/2 will be LMFBR's and the remaining 1/2 will be thermal reactors. Thus, LMFBR's will be generating as much electrical power in the U.S. in the year 2000 as is currently being generated by all sources—hydro, fossil, and nuclear.

Meeting the national energy needs projected to the year 2000 will require a massive engineering effort and capital investment. With this engineering effort and capital investment, the U.S. will be able to meet the energy requirements with economical, clean plants which will have a minimum impact on our environment. It's a challenge as great as the Apollo "Man on the Moon" program and will be as rewarding.

Thus, in the 100 years since Einstein announced the mass-energy relationship, scientists and engineers will have developed systems and constructed plants to meet the energy requirements of man for hundreds of years, utilizing this process. It is expected that fusion development will advance so that before the earth's resources of uranium are depleted, the fusion process can take over from fission and supply the energy requirements for thousands of years in the future with no need for a new discovery to replace Einstein's mass-energy conversion process.
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CE = Combustion Engineering  
CEC = Commission of European Communities  
CEGB = Central Electricity Generating Board (UK)  
CEN/SCK = California Institute of Technology  
CNEN = Comitato Nazionale Per L'Energia Nucleare (Italy)  
EDF = Electricite de France  
EPA = U.S. Environmental Protection Agency  
EPRI = Electric Power Research Institute (USA)  
GAAA = Groupement Atomique Alsacienne Atlantique  
GBRA = Gas Breeder Reactor Association  
H&N = Holmes & Narver  
IFR = Institut fur Reaktorsicherheit  
INR = Institute of Nuclear Research (Poland)  
IVO = Imatran Voima Osakeyhtio (Finland)  
JAPC = Japan Atomic Power Company  
JPL = Jet Propulsion Lab (Los Angeles)  
JTC = Japan Trade Center (Los Angeles)  
KfK = Kernforschungszentrum Karlsruhe  
LASL = Los Alamos Scientific Laboratory  
LLL = Lawrence Livermore Laboratory  
MTC = Mitsui Toatsu Chemicals, Inc.  
NCEC = Nuclear Controls & Electronics Corporation  
NOL = U.S. Naval Ordnance Laboratory  
NRDC = Natural Resources Defense Council (USA)  
PMC = Project Management Corporation (USA)  
PNC = Power Reactor & Nuclear Fuel Development Corporation (Japan)  
RCN = Reactor Centrum Nederland  
SRI = Stanford Research Institute  
TVA = Tennessee Valley Authority (USA)  
UCSB = University of California at Santa Barbara  
UK (DE) = United Kingdom Department of Energy  
W-ARD = Westinghouse Advanced Reactor Division  
W-HEDL = Westinghouse Hanford Engineering Development Labs  
WINB = Western Interstate Nuclear Board (USA)