Fast reactor fuel failures and steam generator leaks: transient and accident analysis approaches
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Fast reactor fuel failures and steam generator leaks: transient and accident analysis approaches
FOREWORD

The early development of liquid metal fast reactors (LMFRs) was conducted largely on a national basis. However, because of costs for the advanced LMFR and constrained budgets, international co-operation is becoming more important. The IAEA is promoting such international co-operation. In R&D, such co-operation plays an important role by allowing the pooling of resources and expertise, thus sharing the high costs of development.

The overall experience with LMFRs has been good and, in many cases, the reactors have performed remarkably well. The fast reactor nuclear technology is a proven technology which is capable of providing nearly unlimited energy supply from the world’s resources of uranium and thorium. It is also capable of converting radioactive waste to more environmentally benign forms. At present, while some countries are reducing LMFR programmes, other countries are planning to embark on their own LMFR development programmes. Under these circumstances, the sharing of scientific and technological information through international exchange is of increasing importance to help countries assimilate LMFR technology. To this end, the IAEA International Working Group on Fast Reactors (IWGFR) has recommended the preparation of an overview of the work on the analyses of LMFR incidents and accidents. The aim of the overview is to analyse the results of experimental and analytical investigations of abnormal (transient) behaviour in the core and heat transport system of LMFRs and to summarize the R&D results. The importance of these topics has been emphasized at many IAEA meetings.

This report consists of a survey of activities on transient and accident analysis for the LMFR. It is focused on the following subjects:

- fuel transient tests and analyses in hypothetical incident/accident situations,
- sodium-water interactions in steam generators, and sodium fires: tests and analyses.

There are also sections dealing with the experimental and analytical studies of:

- fuel subassembly failures,
- sodium boiling, molten fuel-coolant interaction, molten material movement and relocation in fuel bundles,
- heat removal after an accident or incident,
- sodium-water reaction in steam generator,
- steam generator protection systems,
- sodium-water contact in steam generator building,
- fire-fighting methods and systems to deal with sodium fires.
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INTRODUCTION

The need to increase the safety and efficiency of different types of nuclear power plants (NPPs) has resulted in special attention being given to the analysis of hypothetical accident situations. In this respect, sodium cooled fast reactor plants are not exceptions. For any reactor, accident situations involve a severe imbalance between heat generation and heat removal in single fuel subassembly or in the core as a whole. Accident conditions may result from a number of causes: (1) local cooling reduction resulting from flow blockages in a single fuel subassembly with the possible failure propagation to adjacent fuel subassemblies; (2) loss of flow (scrammed or unscrammed) in the core; (3) transient overpower (scrammed or unscrammed); (4) loss of heat sink, and (5) possible combinations of these events. Accidents can pass through a number of stages which have common physical characteristics. The physical characteristics can include: (a) temperature profile disturbance; (b) sodium boiling; (c) fuel pin or whole subassembly failures; (d) molten fuel-sodium interaction, and (e) degraded core heat removal due to debris. In this review, results of experiments and analytical methods for analysing such processes are presented.

In the sodium cooled fast reactor, some incidents result from the fact that sodium reacts with water and air. In this review, experimental data and calculated results of the kinetics and thermodynamics of the sodium-water chemical interaction are presented. An analysis of coolant interactions within free volumes, at small and large water-into-sodium leaks in steam generators is given. An analysis of protection systems for various types of sodium-water steam generators, and a brief review of incident situations in those steam generators is also presented. A concise analysis is given of processes that occur when sodium and water come in contact in the steam generator buildings. This review presents information on the features of sodium burning and on the interaction of sodium with concrete that result from sodium leaks. Information on sodium fire extinguishing methods and systems are also presented as well as an analysis of the process in the fire region.

This review does not provide complete coverage of these subjects, but it gives an overview of the current level of understanding processes that occur during an accident or incident in the sodium-cooled fast reactor core and steam generator. This review should be useful to specialists in the field of nuclear power, and, particularly for those concerned with fast reactors.
1. FUEL TRANSIENT TESTS AND ANALYSES

1.1. EXPERIMENTAL INVESTIGATIONS

1.1.1. Local subassembly faults

As initial events of single subassembly incidents a set of causes leading to the local disturbance of the design power-coolant flow rate ratio within a subassembly, i.e., the disturbance of the heat release and heat removal balance, can be considered. Particular attention has been given in LMFBR safety analysis to cooling disturbances caused by local blockages within a fuel subassembly. The general aim of the studies was to demonstrate that the consequences of a local blockage do not lead to rapid propagation of damage within a pin bundle. Therefore, the aims of these experiments were: (1) definition of the temperature profiles to be anticipated downstream of blockages as a function of various physical parameters, such as flow, power, leakage flow through the blockage and inlet temperature, (2) establishment of the types and courses of boiling events downstream of blockages, (3) assessment of the margin between the onset of boiling downstream of a blockage and the onset of Dryout, (4) determination of the effects of gas in connection with the aims stated above, (5) collection of experimental data to validate codes which enable temperature profiles to be predicted for non-boiling situations, (6) pinpointing of the detection limits of blockages in relation to the capabilities of measuring devices, (7) demonstration whether, in case of decay heat, the blocked region can be cooled by natural convection.

The appearance of a blockage may be related to the presence of oxides in the flow and with coolant impurity [1]. Spacer grids and wire spacers promote collecting of foreign material and give rise to blockage formation, especially in case of wire damage. In this case the subassembly acts like a filter in the circuit (Fig.1.1.1.1a). This blockage picture is typical of an accumulation behind a grid. A failed wire wrap or debris accumulation in a wire wrapped subassembly would lead to a long thin blockage. Blockage appearance can be caused by fuel pin damages either due to manufacturing defects, or design defects, or by improper operating conditions [2, 3].

An analysis of available data reveals that the process of blockage formation of oxides and impurities is very slow compared with the general time scale of failure development. In the case of a cladding fault of one fuel pin the amount of available material is insufficient for global blockage formation. At the same time, a large blockage in the fast reactor core occurs probably only as a result of fuel release from pin cladding [4].

In Japan an experimental study on blockage mechanism [50] was conducted on several kinds of foreign particles using a full size FBR fuel pin bundle, where following results were obtained:

- foreign particles are hardly trapped in the wire wrapped fuel pin bundle. Only the particles of nearly 1 mm diameter are caught under wire wraps (smaller particles go through the pin bundle and larger particles cannot enter into it);
- blockages are permeable,
- blockages do not adjoin from subchannel to subchannel (like checkerboard).

In France the R&D work has been performed supporting design basis subassembly accident analysis [51]. Four classes of the defect causes have been analyzed:
Fig. 1.1.1.1. Schematic diagram of blockage (a) and typical flow pattern (b) behind a central blockage: 1-upper stagnation point; 2-reverse flow; 3-center of the vortex.
defects present at the beginning of life of the S-A subassembly,
defects occurring during irradiation,
particles or objects within the primary circuit,
defects as consequences of combination of the above defects.

Some results of the investigations are as follows:

debris left within the reactor before sodium fill up or introduced during components handling. Particle sizes, after interaction with sodium, of debris collected during last cleanings of SPX1 were up to 6 mm,
corrosion particles. All the available results show that the corrosion particle size is very small (< few tens μm) and with a very low concentration (< few tens μg/kg Na),
products of interaction between sodium and foreign components. Some interaction experiments between sodium and oil or elastomero-rings have been performed at CEA to determine the characteristics of residues. The reaction occurs for sodium temperatures greater than 350°C. The residues are on a coke form, brittle and crumbly. The maximum particle size of residues issued from the interaction between sodium and VITON (elastomeric O-ring) did not exceed 5 mm, but 60% of residues mass consisted of particles less than 0.2 mm in size.

Transport studies in the primary circuit in order to estimate the probability for particles and for objects be carried through primary components from their source to the subassembly inlet have been performed in a frame of ABACUS out-of-pile experimental programme.

Blockage appearance can be caused by oil ingresses in primary circuit, incidents with oil ingresses in sodium took place in PFR and BN-600. Carbonaceous residues on PFR reactor components were identified following an oil ingress into the primary sodium in 1974 [52]. Relatively large particles were found on top of subassemblies withdrawn from reactor just before first criticality. Oil degrades in reactor sodium, whether in the support core or on the surface of the outer pool. The pyrolytic process results in a solid, coke-like structure.

Detailed analysis of the events leading up to the subassemblies outlet temperature rises observed in PFR in June 1991 concluded that the temperature rises are due to partial blockages of the subassemblies inlet filter. The most likely source of debris are degradation products following an oil ingress [53]. Now in Great Britain and Russia the experimental studies were performed for a better understanding of the consequences of the oil ingress in primary circuit. Blockages up to local flow obstruction beyond which cooling is seriously impaired may build up within days or weeks. During this time the fuel released is very likely to be detected. However, in the studies it is assumed that a blockage of certain size has built up which leads to local temperature increase. As a whole, blockages can be classified as local blockages covering a few channels, and global blockages covering a multichannel region. By the place of their occurrence the blockages are classified as those in the inner region and blockages adjacent to subassembly periphery. Blockages can be porous with leakage flow and solid, passed over by coolant, plane and three-dimensional ones. They can be also active (heat generating) consisting of fuel debris, and non-active, formed by particles of pin cladding, corrosion products, etc.
Particular attention has been given to the investigation of hydrodynamic processes in a fuel subassembly with local blockages. It is connected with the determining influence of hydrodynamics on temperature distribution produced in the region of impaired cooling. A number of studies combine the investigation of both hydrodynamics and heat exchange. Experimental studies have been made on subassembly simulators using water and sodium as coolant. A survey of the type and size of the blockages investigated during many years within out-of-pile experimental program are reproduced in Table 1.1.1.1. [22].

Hydrodynamic Studies. Results of many studies have shown that behind a planar blockage in fuel subassemblies with a pitch more than 1.125, there is a quasisteady-state recirculation flow zone. The recirculation zone is separated from the main flow radially by a thin transition zone where there is intensive mass exchange due to turbulence between the recirculation flow and the main flow. The length of recirculation zone is determined by the position of the rear stagnation point (Fig. 1.1.1.1b). A degree of a flow disturbance behind the blockage is determined to a large extent by its size, shape and location. A characteristic property of the small local blockage is fast restoration of flow in the wake. The flow represents a number of interacting streams. In most studies the hydrodynamics of large blockages is investigated. Fig. 1.1.1.2 presents experimental data on velocity and pressure distributions behind the blockage [5]. The center of the recirculation vortex is at a distance of \(~0.5 \, D_b\). The pressure in the blocked region is lower than in surrounding channels, a minimum of static pressure being observed at the blockage boundary. In the blockage wake a region of negative dynamic pressure is observed. From a qualitative point of view the picture of the flow is similar to the wake of a circular blockage in a free flow.

The size of the disturbed flow region behind a blockage and, respectively, the extent of the blockage effect upon the velocity field in a subassembly, is characterized by the recirculation zone length. Experimental and analytical data point to the tendency towards an increase of the recirculation zone length with increasing Re_b [5, 6, 7, 35]. At Re_b > 10^5, the recirculation zone length does not depend on Re_b. Investigations for the central blockage reveal an increase of the recirculation zone length with an increase of the blockage size. At a blocked flow area more than 20% an increase of the recirculation zone length is slowed down (Fig. 1.1.1.3) [5, 6, 35]. The data [7] reveal that the presence of grids in the wake zone behind a blockage results in a considerable decrease of its length. This is explained by more intensive axial and lateral turbulent exchange that plays an important part in velocity field formation behind blockage. The length of the reverse flow region in the wire-wrapped fuel subassembly is assessed as 1.3-2.6 diameters of blockage in case of a central blockage and as 1.3-4.8 blockage diameters for a corner blockage. The total length of the disturbance of flow behind blockage exceeds the size of the recirculation zone by 5-10 times [14, 33]. Experimental studies [8] and analytical assessments [9] show that the reverse flow velocity is proportional to the inlet flow velocity and to the relationship \((2 - \beta)(1 - \beta)\), where \(\beta\) is the blocked flow fraction of a subassembly. A maximum value of the reverse flow velocity can reach 0.7 of main flow velocity. The residence time of coolant in the recirculation zone behind blockage found from the results of measurements of tracer concentration in the wake is inversely proportional to the flow velocity for the blocked section but depends on an blocked flow area (Fig. 1.1.1.4). The residence time of coolant behind a corner blockage is \(~2.4\) of the residence time behind the central blockage.
<table>
<thead>
<tr>
<th>No</th>
<th>Blockage Type</th>
<th>Size</th>
<th>Coolant</th>
<th>Investigations</th>
<th>Ref.</th>
</tr>
</thead>
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<td>67%</td>
<td>Water</td>
<td>- single-phase temperature distribution</td>
<td>8</td>
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<tr>
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<td></td>
<td>Sodium</td>
<td>- single-phase temperature distribution and boiling behaviour</td>
<td></td>
</tr>
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<td>8</td>
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<tr>
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<td></td>
<td>Sodium</td>
<td>- single-phase temperature distribution and boiling behaviour</td>
<td></td>
</tr>
<tr>
<td>3</td>
<td>Central impermeable</td>
<td>15%</td>
<td>Water</td>
<td>- dimensions of the wake</td>
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</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td></td>
<td>- mass exchange between wake and main flow</td>
<td>9</td>
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<td></td>
<td></td>
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<td>- single-phase temperature distribution</td>
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<tr>
<td>4</td>
<td>Central impermeable permeable</td>
<td>41%</td>
<td>Water</td>
<td>- dimensions of the wake</td>
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</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td></td>
<td>- mass exchange between wake and main flow</td>
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<td>47%</td>
<td>Water</td>
<td>- dimensions of the wake</td>
<td>10</td>
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<td>- mass exchange between wake and main flow</td>
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<td>- single-phase temperature distribution</td>
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<td>41%</td>
<td>Water</td>
<td>- phenomenological study of the flow in the wake</td>
<td>9</td>
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<td></td>
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<td></td>
<td>- influence of the pins and the grid on the flow in the wake</td>
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<td></td>
<td>- boiling behaviour up to dryout</td>
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<td>- influence of gas in the wake</td>
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<td>- behaviour of gas in the wake</td>
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<td>- influence of a grid in the wake</td>
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<td>Blockage variable in size and porosity</td>
<td>10-70%</td>
<td>Water</td>
<td>- single-phase temperature distribution dependent on the size and porosity of the blockage</td>
<td>26</td>
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</table>
Fig. 1.1.1.2. Velocity and pressure distribution behind blockage
Fig. 1.1.1.3. Dependence of the recirculation zone length behind central (a) and edge (b) blockage on blocked flow area.
Fig. 1.1.1.4. Dependence of the residence time of coolant on a blocked flow area

The pressure drop near the blockage is found by taking into account flow rearrangement at the blockage periphery [10]:

$$\Delta P_b = \frac{G_c^2}{2\rho \omega_c} (1 - \beta) \left[ f \frac{L}{D_b} + 0.8\beta + 0.6\beta^2 \right]$$

(1)

where $\Delta P_b$ is pressure drop across the blockage section, $\omega_c$ - subassembly flow area, $D_b$ - blockage diameter, $\rho$ - coolant density, $f$ - resistance coefficient, $G_c$ - coolant flow rate through a subassembly.
Experimental and calculation data reveal that the total flow rate in a subassembly is reduced no more that 15% if the blocked flow area does not exceed 0.7. Data by different authors agree satisfactorily (Fig. 1.1.1.5) and can be described with an accuracy of 6.5% by the following relationship [11]:

\[ \frac{G_b}{G_0} = 1 - \beta^6 \]  

(2)

where \( G_0 \) - coolant flow rate through a subassembly without blockage.

In a permeable blockage the wake geometry and the flow in the wake are influenced by the leakage flow through the blockage. The leakage flow through the blockage is defined as the ratio of the flow through the blocked area to the flow through the same area that would prevail in the absence of the blockage. In Fig. 1.1.1.6 an example of the influence of the leakage flow on both the length of the reverse flow region and the reverse flow velocity is given for a 21% corner blockage (results from [22]). It is seen that the leakage flow greatly influences the distance between the blockage and the bottom of the recirculating flow, but has little influence on the length of the wake region (\( L_R \)). With increasing leakage flow the vortex becomes shorter and the reverse flow velocity decreases about linearly. At a leakage flow of about 11% the reverse flow ceases. This behavior was observed for all size of blockages. However, the leakage flow at which the reverse flow ceases is dependent on the size of the blockage. For the central and corner blockages the rate of leakage flow at which the reverse flow ceases is shown in Fig. 1.1.1.7. In both cases this leakage flow increases about linearly with the size of the blockage. Because of the higher reverse flow velocity in the central blockage case the leakage flow necessary to interrupt the reverse flow is much higher than in a corner blockage. It is important to realize boundary conditions because blockage formation by particles always starts at high porosity corresponding to a high leakage flow.

Temperature field investigations. An analysis of data [9, 13] reveals that the temperature distribution downstream of a blockage is determined mainly by flow pattern and, especially, by the formation of a reverse flow region. Therefore, the temperature distribution depends directly on the parameters influencing reverse flow. The fuel pin cladding temperature reaches a local maximum in the blockage region or at a distance of ~50% the blockage diameter behind the blockage (Fig. 1.1.1.8) [9, 13]. Temperature field restoration initiates simultaneously with restoration of the velocity field. The temperature profile is restored at a distance of ~8 central blockage diameters, i.e. at a distance about 4 times more than the recirculation zone [14]. Figures 1.1.1.9, 10 show the temperature distributions in the form of normalized isotherms (\( \theta = \Delta T / \Delta T / dz \)) in the wake of the 49% central and 21% corner blockages within subassembly with the spacer grids [22]. The peak temperature occurs in the region of the vortex center. In all the tests with the 49% central blockage the temperature distributions were found be very similar. A pronounced influence of the test parameters was not observed. There exists, however, little dependence upon the flow velocity and the power to flow ratio.

In contrast to the central blockage the temperature field behind the 21% corner blockage is dependent on the main flow velocity. With decreasing main flow velocity the temperature rise also decreases and the region of high temperature moves towards the blockage (Fig. 1.1.1.11). It is caused by the reduction of the length of the recirculating zone and the reverse flow velocity. The influence of the power to flow ratio was small in every case.

Experimental and calculated data of various authors on coolant temperature rise behind the blockage reveal an increase of rise with an increase of the blocked flow area up
Fig. 1.1.1.5. *Dependence of relative flow rate in a subassembly on a blocked flow area*

**Fig. 1.1.1.6.** *Influence of the leakage flow on the wake geometry and reverse flow velocity*
to 0.3, then some decrease of rise (Fig. 1.1.1.12). Data on coolant temperature rise behind the central blockage are described to an accuracy of ±11% by the following relation:

$$\frac{(t_{\text{max}} - t_o) \rho C_p w}{q \cdot Re^{0.25}} = 150 \sin 2\beta \exp(-2.25\beta)$$  \hspace{1cm} (3)

where $t_{\text{max}}$ is a maximum coolant temperature behind the blockage, $t_o$ - coolant temperature before the blockage, $q$ - heat flux, $C_p$ - coolant heat capacity, $w$ - mean coolant velocity in a subassembly, $Re$ - Reynolds number. The average temperature rise in the wake can be determined in terms of the coolant residence time in the wake by the following relationship:

$$\bar{t} - t_o = \frac{q_v \tau}{\rho C_p}$$  \hspace{1cm} (4)
The ratio of the maximum coolant temperature rise to the average temperature rise behind the blockage (K) does not depend on coolant velocity and the heat flux value for the central blockage it is $\sim 1.5$ and for the lateral blockage $\sim 1.4$ (Fig. 1.1.1.12). So, the maximum coolant temperature rise behind a blockage can be evaluated by the relation

$$
\frac{t_{\text{max}} - t_0}{t_0} = \frac{q_v \tau}{\rho c_p k}
$$

The coolant temperature rise behind the lateral blockage is 1.5-2 times more than superheating in the case of an equal central blockage. The cause of this effect is a considerably less mass and heat exchange in case of an edge blockage compared with the central one.

Data on coolant temperature rise behind the blockage in a subassembly show considerably less rise values than for smooth pin bundles [15, 16]. In case of a porous blockage even a small leakage flow of coolant reduces a temperature rise of coolant and, respectively, of fuel elements behind the blockage [17, 18]. Figure 1.1.1.13 shows the variation of the maximum temperatures at different axial measuring planes versus the leakage flow rate for the fixed corner blockage [22]. The following statements, which are valid only for this 21% corner blockage, can be derived from this figure:

- the reverse flow disappears at around 7% leakage flow,
- the temperature distribution is greatly influenced by the leakage flow,
the peak temperature remains nearly constant for leakage flow rates less than 4%, for leakage flow rates greater than 4%, the peak temperature decreases sharply reaching about half of its maximum value as the reverse flow disappears.

---

**Fig. 1.1.1.9. Normalized temperature field behind the 49% central blockage**

Experiments under natural convection conditions have also been performed [22]. Although the results of these experiments may not be directly applied to real reactor conditions they are important because they shown that in the case of natural convection the temperature rise due to the blockage is small compared with that under normal flow.

1.1.1.2. Out-of-pile experiments under boiling condition

Sodium boiling in fast reactor fuel subassemblies can occur in the case of substantial blocked flow area. The main problem is in this case to find the limits of fuel elements cooling in a boiling zone behind blockage, to determine the character of the two-phase flow development. However, besides thermohydraulic factors, this process is affected by the geometry of a fuel subassembly and of the circuit as a whole that considerably hampers out understanding of experimental results. For their interpretation, complex three-dimensional two-fluid calculation codes are necessary now.

*Local boiling behind a blockage.* In earlier tests on local boiling behind a blockage in a 7-pin bundle [19] investigators did not observe instability appearance as subchannels near
the wall remained filled with subcooled liquid and the pressure drop was unchanged. Larger scale experiments were required for detailed understanding of conditions for changing over from local boiling to pool boiling, so that to simulate radial temperature distributions in a prototype.

Such tests were undertaken at PNC on a 37-pin bundle [20]. Experiments were carried out in a sodium loop on a bundle with a blockage of 29% in the center and of 50% at the bundle flow periphery. The pin diameter was 6.5 mm, pin pitch - 7.9 mm, heated zone length - 455 mm, axial spacer pitch - 265 mm. The inlet coolant temperature was 461°C, coolant velocity - 1.78 m/s and heat flux at the initial time period was 127 W/cm².

For the central blockage at the initial time no boiling was observed. At 16 seconds, when the heat flux was increased up to 135 W/cm², local boiling appeared directly behind the blockage region. With the power increasing up to time 70 s, the boiling region increased in size and continued to increase later on (Fig. 1.1.1.14). However, the dryout region was locally limited, and a decrease of flow was hardly perceptible. Oscillations of temperature and pressure were observed from the moment of boiling initiation. At partial blockage in the peripheral region of a subassembly, coolant boiling was observed in the center of the recirculation zone and, in the course of propagation, did not cover for a long time the region immediately adjacent to the blockage surface. A value of superheat required for coolant boiling initiation did not exceed 30°C and decreased with increasing flow velocity in a fuel
Isotherms, normalized temperature rise $\theta \,[\text{m}]$

Run 278
$q = 100.8 \,\text{W/cm}^2$
$v_o = 4 \,\text{m/s}$

Run 276
$q = 50 \,\text{W/cm}^2$
$v_o = 2 \,\text{m/s}$

Run 274
$q = 24.8 \,\text{W/cm}^2$
$v_o = 1 \,\text{m/s}$

Run 273
$q = 14.8 \,\text{W/cm}^2$
$v_o = 0.6 \,\text{m/s}$

Fig. 1.1.11. Influence of main flow velocity on the temperature field
Fig. 1.1.1.12 Data on temperature rise behind different blockages
In [9] the acoustic noise characteristics, outlet flow fluctuations and the frequency of bubble formation at local boiling behind a blockage were studied. At the beginning of boiling, a sharp change of flow rate accompanied by oscillations was observed. These oscillations point to repeated bubble formation and collapse with a definite frequency. The growth of bubbles formed is substantially retarded due to the presence of subcooled liquid around them. So, the void meters cannot detect incipient boiling until considerable numbers of bubbles are formed. With increase of heat flux the pin surface temperature grows and reaches the sodium saturation temperature. Sharp fluctuations of the flow rate are observed at the outlet due to generation of great numbers of vapor bubbles. The pin temperature raises rapidly, which is indicative of residual liquid film dryout. With increasing heat flux the bubble grows and the generation frequency decreases. The frequency of bubble formation also decreases with increasing bubble diameter. The value of the bubble diameter is affected by coolant velocity. The frequency and equivalent diameter of bubbles are related by 

\[ \frac{d_w}{d_L} = 139 \text{ mm/s} \]

at a velocity of 0.14 m/s. Sodium boiling causes an increase of coolant flow rate fluctuations at the subassembly outlet up to 1.17 m/s. By means of measuring the flow fluctuations, local sodium boiling behind the blockage can be detected.

Three types of transient processes from incipient boiling to the onset of dryout have been identified: 1) dryout takes place at oscillatory boiling; 2) dryout follows changing over from oscillatory boiling conditions to steady-state ones; 3) dryout occurs also during steady-state boiling irrespective of the presence of oscillatory boiling. Dryout does not necessarily take place in a zone with irregular nucleate boiling fluctuation (up to 200 W/cm²).
Related to the different measuring planes

Max. normalized temperature $\Theta$ [m$^2$]

Leakage ratio $L$ [%]

Distance of the measuring plane from the blockage

$\nu_0 = 5 \text{ m/s, } q = 27 \text{ W/cm}^2$

Fig. 1.1.1.13. Influence of the leakage flow through the 21% corner blockage in water
Fig. 1.1.1.14. Boiling region and isotherms behind the central blockage
but margin of coolability in a locally blocked pin bundle is 20±30% increase between the onset of boiling and dryout in terms of the power/flow ratio. For transient conditions, temperature gradients near the two-phase flow region are characteristic. Similar investigations were carried out by Huber and Peppier at Karlsruhe [21, 22] for 49% center and 21% corner blockages within a 169-pin bundle. The results from the two boiling experiments with the 49% central and the 21% corner blockages can be summarized as follows: the boiling regimes are different for the two blockages, if normal operating velocity in the subassembly is assumed. For the 49% central blockage at flow rates of \(1.5 < v_0 < 2\) m/s oscillating boiling dominates. The margin between the start of boiling and the onset of dryout in oscillatory boiling corresponds to a 100% increase of the power/flow ratio. At low flow rates (\(\leq 1.5\) m/s) rapid development of the boiling region into a steady-state vapor cavity results in a small dryout margin (\(\approx 20\%\) increase in power/flow ratio) between the start of boiling and the onset of dryout. The volume of pins at risk of melting at the onset of permanent dryout is estimated to be in the range of 50 to 100 cm\(^3\). For the 21% corner blockage the generally observed boiling pattern is steady-state. The margin between the onset of boiling and permanent dryout lies in a range corresponding to a 40-60% increase in the power/flow ratio. However, brief dryout occurs much earlier. The excess temperature is only half that for the 49% central blockage. This illustrates that the oscillating boiling regime is more efficient at removing heat. The volume of pins at risk of melting is estimated to be in the range of 15 to 50 cm\(^3\). Under normal SNR 300 Mk Ia operational power/flow conditions local boiling would occur behind the 21% corner blockage, but not behind the 49% central blockage of the two types investigated. Figure 1.1.1.15 illustrates a boiling process behind a blockage.

In [30] the critical heat flux data under stable boiling patterns were analyzed and were correlated with both the boiling inception heat flux and the core flow velocities (Fig. 1.1.1.16). These data are obtained from the experiments carried out at PNC (20% edge blockage in 37-pin space-gridded bundle) and at KFK (21% corner blockage in 161-pin space-gridded bundle). The shaded zones represent the stable blocking region with no pin melting. Studies carried out in [23] revealed that some differences between calculated and experimental results are observed which can be explained by complex two-phase flow analysis. These analyses require the use of large numbers of parameters, the values of which should be refined from experimental studies of two-phase flows structure in fuel subassemblies.

1.1.1.3. The effect of gaseous fission products release

Due to the possibility of pin failure combined with fission gas release into the recirculating zone behind the blockage, the behavior of gas and its influence on cooling of this zone were investigated [21, 22, 24, 26, 30]. In [21, 22] all experiments showed the general behavior for the conditions under which gas accumulation arises. These are: (1) below a minimum main velocity, gas does not accumulate, and (2) at flow velocities higher than this threshold the gas rate, which is necessary to produce gas accumulation, increases exponentially with increasing main flow velocity.

Fig.1.1.1.17 shows the conditions under which gas accumulates for the 21% corner blockage. The gas accumulates in the hatched zone. The minimum flow velocity is about 2.2 m/s and the minimum gas injection rate about 3 cm\(^3\)/s. For the 49% central blockage the minimum flow velocity is about 1.2 m/s. Since in this case gas could only be injected into the main flow upstream of the blockage the minimum gas rate was considerably higher (\(\sim 250\) cm\(^3\)/s) than that shown in Fig. 1.1.1.17. However, the general behavior of gas accumulation
Fig. 1.1.1.15. Diagrams illustrating liquid and vapour flows behind blockage.
velocity in the recirculating flow, must at least be of the same quantity as the rising velocity of the gas bubbles due to buoyancy. The differences of the corresponding minimum main flow velocities are to be explained by the different geometries of the blocked area and, hence, by the different ratios between reverse and main flow velocities. At recirculating velocities than the rising velocity due to buoyancy, gas bubbles are transported towards the blockage and reach the wake center by centripetal forces where a gas cavity is formed. In all experiments the minimum reverse flow velocities are similar and nearly independent to the size and type of blockage. The mean value is 0.55 m/s.

Six series of experiments were conducted at PNC [24, 26]. The 37-, 61- and 91-pin bundle test section were used in the experiments. The simulated fuel subassembly are named as "37GC", "37GE", "37WC", "37WE", "61WC" and "91WE" depending on the spacer type (G: grid spacer, W: wire spacer) and blockage location (C: central, E: edge). The common dimensions are the pin diameter of 6.5 mm and the pin pitch of 7.9 mm. The same as Monju’s fuel subassembly, Haga et al. [26] have explained the behavior of gas behind blockage as follows. When the flow velocity is less than the lower critical velocity, the reversal flow in the wake is very low or almost stagnant. Hence the released gas smoothly flows out of wake and a gas cavity is not formed. On the other hand, if the velocity is higher than the upper critical velocity, the cavity would be finely divided by the shearing force of coolant and small bubbles would circulated in the wake. Only when gas cavities are formed in the wake of the blockage does the cooling capability decreases extensively. Figure 1.1.1.18 shows the dependency of the normalized temperature rise behind central and edge
Fig. 1.1.1.17. Conditions of gas accumulation in sodium blockages on sodium velocities under gas release conditions. One can see, there are low velocity regions (less than 2 m/s) in which gas release does not cause additional temperature rise. On the other hand, in the higher velocity region (> 5 m/s) the temperature rises saturate or decrease. These facts are consistent with those obtained at KfK [22]. Fig. 1.1.1.19 presents a correlation between the gas release rate and the normalized temperature rise under high coolant velocities (practical limit of normal reactor operation). The normalized temperature rise increases greatly with increasing gas release rate for \( G_g < 1.0 \) g/s, while for \( G_g > 1.0 \) g/s the temperature rise slightly decreases with increasing the gas release rate. The similar tendency is measured in other test geometries. Thus, the relation between the gas release rate and the temperature rise can be classified into two stages.
Empirical correlations to estimate the normalized temperature rise were obtained in each stage [26].

In first stage, when the gas release rate is small \((G_g/V_{acc} < 0.005 \text{ g/s cm}^2)\), the relation for both, central and edge blockage, is the following

\[
\frac{\Theta_{2p}}{\Theta_{1p}} = 1 + 300 \frac{G_g}{V_{acc}}
\]

where \(\Theta_{1p}, \Theta_{2p}\) is single and two-phase normalized temperature rise (without and with gas), \(V_{acc} = 2 A_b D_b\), \(A_b\) is the blockage flow area, \(D_b\) - length diameter of blockage. In second stage the saturated value of \(\frac{\Theta_{2p,sat}}{\Theta_{1p}}\) under the coolant velocity of 5 m/s are expressed as:

\[
\frac{\Theta_{2p,sat}}{\Theta_{1p}} = 1 - \left(0.017/D_b [m]\right)
\]

for central blockage,

\[
\frac{\Theta_{2p,sat}}{\Theta_{1p}} = 1 - \left(0.017/2D_b [m]\right)
\]

for edge blockage

These empirical formulas cannot be directly applied for an actual fuel subassembly due to differences in density of gases used in tests and in reactor operation conditions. Therefore, a procedure for evaluating temperature rise behind blockage due to fission product gas in an actual fuel subassembly has been recommended [26]. The general effects of gas release behind a blockage were revealed from the above steady release runs. However, in the case of gas release in a fuel subassembly, gas would be released transiently. The transient phenomena may add an unexpected effect on the thermohydraulics behind a blockage under gas release condition. To examine this effect, several transient gas release tests were conducted [31].

The following summarize the results of the transient tests: (a) the maximum temperature rise during transient gas release was approximately the same as that during steady release runs, (b) the temperature rise is influenced solely by the gas volume and gas concentration accumulated in the wake and is independent to the momentary gas release rate.

The fact that there was no fundamental difference between the peak temperatures under transient gas release and those under steady gas release, shows that the plentiful steady release run data can be used for the evaluation of the thermal effect of fission product gas release in a fuel subassembly.

1.1.1.4. In-pile experiments under single-phase and boiling conditions

Two in-pile local blockage experiments, MOL 7C/6 and 7, have been performed in the BR2 reactor at SCK/CEN MOL/Belgium [25]. The particular objective of these tests was to investigate the consequences of local faults at a high burnup and of blockages having contact with the subassembly wrapper tube. (These experiments were part of a large programme of seven tests to investigate local faults in fast reactor subassemblies). The in-pile section comprised the complete sodium circuit enclosed in a pressure tube (Fig. 1.1.1.20). The main components of the sodium circuit are: the test section with the fuel pin bundle, the sodium pumps, the sodium-helium heat exchanger and an expansion tank. The overall length
Fig. 1.1.1.18. Dependency of inside wake temperature increases on sodium velocities gas release conditions
of the in-pile section was 8.5 m, the diameter varied from 122 mm for the test section to 450 mm at the top. The test section was surrounded by a cadmium screen to harden the neutron spectrum. The central part of the loop, the so-called test train, contained the bundle structure, i.e. spacer grids and dummy rods, and the local blockage. After insertion of the fuel pins, the test train was loaded in the central channel of the reactor.

The MOL 7C/6 fuel pin bundle was composed of 30 pre-irradiated UO₂-PuO₂ fuel pins and seven dummy pins. The fuel pins had a length of 157 mm and a diameter of 6 mm. They were spaced by honeycomb grids with a pitch of 7.9 mm (p/d = 1.32). The fuel bundle contained a local blockage (Fig. 1.1.1.21) located approximately at the level of maximum power. The local blockage could be cooled from inside by sodium flowing through the central dummy pin. This additional cooling could be interrupted by closing a pneumatic valve (local blockage valve, LBV) at the inlet to the central dummy pin. In this way conditions were created inside the bundle that led to sodium boiling, dryout and fuel pin failure (transient phase of the experiment).
The MOL 7C/7 bundle consisted of 19 fresh UO$_2$-PuO$_2$ fuel pins. The pins had a length of 1562 mm and a diameter of 7.6 mm. Spark eroded grids were used as spacers. The pitch was 8.8 mm (p/d = 1.16). The blockage was in contact with two walls of the hexagonal wrapper tube (Fig. 1.1.1.21). There was no additional cooling of the blockage.

In both experiments, the inside of the blockage was filled up with highly enriched UO$_2$ spheres coated with a Chromium layer of about 7 μm. The diameter of the spheres was 0.42 mm. The porosity of the blockage was approximately 40%.
The central blockage (MOL 7C/6). Initial experimental conditions were as follows:

- flow rate through a subassembly: 1.55 kg/s
- inlet sodium velocity: 2.02 m/s
- inlet sodium temperature: 393°C
- outlet sodium temperature: 688°C
- maximum linear power in an average element: 398 W/cm.

At 44 seconds after closing the local blocking valve the reactor was shutdown by a scram due to a temperature rise in the bypass line.
The development of important signals during the transient is presented in Fig. 1.1.1.22. After blocking (event N 1) the temperature behind the blockage (TE 35 and TE 37) quickly rose to the sodium boiling temperature (about 1010°C). The onset of boiling (event N 2) has been recorded at time 3.4 s, dryout (event N 3) has been registered by the thermocouples TE 37 and TE 35 as a fast temperature rise. The first cladding failure (event N 5) was registered at time 7.2 s at a maximum temperature behind the blockage of about 1200°C (TE 37). In about 1.4 s after cladding failure the signal in the Delayed Neutron Detection (DND) system increased substantially. At time 7.7 s the melting temperature of steel (1400°C) was reached. Within the next 3 s the melting zone was spread to nearly the whole region of the blockage.

The corner blockage (MOL 7C/7). At reaching the nominal power one of the thermocouples (TE 66) registered boiling. Fifteen minutes later the sodium flow was reduced gradually (1% min). After a total reduction of 7%, important fuel pin failures were evidenced by the DND signals. In two days the cooling conditions in the blockage region varied again. Boiling in the middle part of the blockage was observed. Parameters variation is shown in Fig. 1.1.1.23. To intensify the process of boiling, the coolant flow was reduced from 15.15 h on (event N 3). After a change by 4.5%, all three thermocouples (TE 65, 66, 67) recorded boiling in the top volume behind the blockage. As this did not result in dryout or fuel pin damage, the coolant flow was further reduced from 15.34 h on. 2 min later, after a change of flow of only 2.5%, the slowly starting rise in the blockage temperatures (TE 67) and in the DND signal indicated the onset of damage propagation in the blockage (event N 6). The maximum DND signal (event N 8) was recorded at 15.44 h, i.e. 8 min after the first
Fig. 1.1.1.23. Mol 7C/7-Blockage temperatures and DND signal during the first transient

dryout. Damage propagation during the first transient was limited to the zone of the blockage. Towards 16 h, i.e. 24 min after the onset of damage propagation, the first transient was completed by establishment of new stable blockage configuration. The second transient took place after the irradiation at fuel power during the next 38 h. It was faster and more violent without any initiation from outside. The DND recorded the highest value measured in the experiment MOL 7C/7. The conditions stabilized in 6 min and the irradiation was continued over another five hours until controlled manual shut down. The results of
experiments are under evaluation and interpretation. From a preliminary interpretation it may be concluded that there is no fast failure propagation.

CONCLUSIONS

1. The analysis of the available experimental results has allowed to obtain approximate evaluations of the main characteristics of velocity and temperature fields in subassemblies with a blockage for single-phase flows: recirculation zone length, coolant flow distribution in a subassembly, coolant temperature rise in the wake etc. At the same time it is clear that currently available data provide only rough estimates of thermohydraulic characteristics of a subassembly in case of a blockage depending on the value of Re number and of the blocked flow area β. The full range of values is not covered and, for example, there is little data related to small blockages. The greater part of data were obtained for blocked subassemblies of "smooth" pins. As the rate of lateral heat and momentum exchange in subassemblies with wire spacer is about an order higher than in "smooth" bundles, then it should be expected that hydrodynamic characteristics of the blockage wake region will appreciably differ in this case as compared with the data for smooth bundles.

2. The in-pile and out-of-pile results obtained demonstrate that boiling behind a blockage does not lead to fast propagation of a failure in pin bundles.

3. Gas release behind a blockage as a result of pin failures may be the cause of further pin damage in a fuel subassembly.

4. An investigation of the mechanisms of the effect of various factors on temperature fields distribution (the flow rate or energy release variation, the values of Reynolds and Peclet numbers and the fraction of the blocked flow area of a subassembly) calls for carrying out additional systematic experimental and analytical studies. Special attention should be given to the investigation of thermal hydraulics of subassemblies with porous and energy generating blockages, as well as to the initiation conditions for dryout at boiling behind a blockage and of subassembly cooling limits.

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39
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1.1.2. Sodium boiling physical phenomena

Sodium-boiling phenomena play an important role in the evaluation of accident scenarios taken into account in the safety analysis of LMFBR essentially for two reasons: (1) the void rate of the core is one of the parameters controlling the neutron flux during the accident and (2) boiling may lead to dryout and irreversible melting of materials.

Some specific problems arise at coolant boiling in fuel assemblies. Non-uniformity of coolant heating over the fuel assembly cross-section leads to boiling, arising initially only at some fuel elements whereas in the rest the flow remains one-phase. Vapor generation in some part a fuel subassembly, taking into account the large difference in specific volumes of vapor and liquid, is a strong disturbing factor causing a drastic change in hydrodynamics of the whole flow as a result of which the flow in subassemblies becomes unstabilized. An investigation of the consequences of possible accidents leads to the problems of fuel pin cooling conditions at reduced flow rates or under natural circulation operating conditions, at partial or total steaming of a subassembly or a reversal of circulation in a fuel subassembly. The solution of these problems calls for a non-stationary consideration of the processes.

1.1.2.1. Experimental investigation methods

Experimental studies of heat exchange processes in fuel subassemblies are performed, as a rule, on bundles having electrically heated pin simulators and containing a small number of fuel elements (1-61), and, in some extreme cases, in fuel-scale subassemblies. Two methods of transients simulation are used: an increase of energy release of the fuel element simulators at constant inlet coolant temperature and flow rate through the subassembly; and a decrease of flow rate through the subassembly at fixed power up to the onset of coolant boiling. It should be noted that all the test facilities used were well instrumented. This has allowed us to gain a clear idea of the incipient boiling process and of further propagation of the boiling process within the model subassemblies. In the following paragraphs we discuss the principal features of typical experimental configurations of various types.

The annular channel. First out-of-pile investigations of incipient boiling of sodium were carry out in a single-pin annular channel [1]. The subassembly simulator was a tube with an inside diameter of $\varnothing$ 9.8 cm into which an electrically heated pin with an outside diameter $\varnothing$ 6.6 mm and 750 mm long was inserted. The heated section was 600 mm the maximum heat flux - 300 w/cm$^2$ and the maximum permissible surface temperature 900°C. The mock up was mounted on the sodium circuit with a maximum flow rate of 50 l/min. and a maximum coolant temperature of 750°C. Sodium boiling was achieved by means of a gradual increase of heating rod power at constant coolant inlet temperature and flow rate through the channel. The diagram of the mockup and points of detector positioning are shown in Fig. 1.1.2.1. The sodium inlet and outlet temperatures were measured with the use of cromel-alumel thermocouples. Three thermocouples were placed into the heating element cladding at a various height for monitoring of the surface temperature of the element. Eight vapor meters were used to indicate the onset of coolant boiling and to define further propagation of vapor bubbles. The sodium inlet and outlet velocities were determined from electromagnetic flowmeters indications. Three pressure gauges placed at various locations of the section were used for the recording of pressure pulses at coolant boiling. The initiation of boiling was determined from the beginning of sodium inlet temperature pulses and the real onset of boiling was ascertained by indications of vapor-content detectors, flow meters and pressure gauges. In [2] there were both tubular and annular test sections which were used to investigate fundamentals of liquid metal boiling thermohydraulics. With direct electrical
Fig. 1.1.2.1. Schematic diagram of the sodium boiling test sections. AC - accelerometer; M - microphone; $P_N$ - pressure transducer; BD - acoustic sensor; T - thermocouple; $\Pi$ - void meter
heating, heat flux of up to 300 W/cm\(^2\) was available. In the annular test section, indirectly heated heating elements of up to 250 W/cm\(^2\) were employed.

7-pin model. The next stage in investigating sodium boiling processes was a study of transient phenomena in 7-pin subassembly simulator [3]. Seven electrically heated pins are assembled into a bundle and installed in a hexagonal duct. The elements were \(\varnothing 6.5\) mm in diameter and 1.515 m long. Each pin was wrapped by a 1.3 mm diameter spacer. The main parameters of the mockup corresponded to those of the MONJU prototype fast reactor. The diagnostic instrumentation used in the model included the following main elements (Fig. 1.1.2.1b): 62 thermocouples were used for measuring the fuel element surface temperature; the coolant temperature was measured by 7 thermocouples built into spacer wire; 4 thermocouples monitored the duct surface temperature; 2 thermocouples were mounted at the inlet and outlet; 14 vapor-meters were used for the determination of the boiling process parameters; acoustic noise arising at boiling was measured by various type acoustic detectors (an acoustic sensor, a microphone, an accelerometer). Another 7-pin bundle simulator is described in [4].

19-pin bundle. After studying transient conditions in 7-pin bundle mockup in Japan a 19-pin model subassembly was proposed for studying loss-of-coolant accidents [4]. 19 fuel pin simulators were assembled in a tight bundle and installed in a hexagonal duct. To simulate loss-of-coolant conditions there were used: (a) an inertia method when flow rate through the subassembly was decreased similar to inertia rotation of a pump after loss of power, (b) a method of a sudden pump switch-off: pump inertia motion caused by an accident with a main pipe rupture was simulated. Flow rate through fuel subassemblies was quickly decreased by means of discontinued power supply to the pump. Coolant flow rate through the subassembly was maintained due to natural connection after a pump switch-off, and (c) a method of valve closing: an accident with inlet blockage was simulated by means of closing the inlet valve. Coolant flow rate through the test section was fully discontinued.

A 19-pin bundle mockup was used to investigate steady-state boiling and the results have been applied to slow transient-boiling instability analysis [6]. The test section consisted of a bundle (60 cm heating length and 50 cm prolongation) and an outlet tube with 2 cm internal diameter and 100 cm in length. Experiments were performed for various power levels (1, 2, 3, 5, 8, 10, 12 and 16 kW per pin).

37-pin bundle. The most important experiments, especially with view to three-dimensional effects in the boiling behavior, were carried out in a 37-pin bundles. The test section, i.e. the KNS 37-pin bundle, was adapted to the thermal hydraulic conditions of a fuel element of the SNR 300 MK 1a core [7]. The 37-pin bundle is represented schematically in Fig. 1.1.2.2 together with the axial power profile and with the bundle cross-section. The pins used were electrically heated rods with sinusoidal heat flux distribution with a maximum pin power of 320 W/cm. It was also possible to simulate a power tilt according to the radial power distribution in the core. The pin diameter, the pin pitch, the heated and the upper unheated length, which simulate the upper breeder zone, are identical to the corresponding data of the fuel element of the SNR 300. The lower part of the test bundle is shortened compared to that in the reactor. The missing frictional loss of this part was compensated by a throttle valve and the coolant inertia by an equivalent pipe length at the test section inlet. To measure temperature, pressure and flow and to detect vapor bubbles, the test section was equipped with about 240 measuring points. With a view to the three-dimensional behavior, the temperature and void instruments within the bundle were positioned along the 90°-270°, and the 0°-180° axis of the cross-section. The errors of
measurement were $\pm 2$ K for coolant temperature, $\pm 2.7\%$ for mass flow and $\pm 0.009$ bar for pressure.

In Fig. 1.1.2.3 the compact sodium boiling loop is shown. This loop was designed for sodium boiling tests with bundles operating under conditions similar to those of sodium cooled reactors. The pump tank with its free surface simulated the upper plenum. The mass flow through the test section and bypass and the desired pressure drop were adjusted by the throttle valves V1-V3. The flow run-down characteristic being an essential feature of the experiments was adapted to that of the SNR 300 by adding a supplementary gyrating mass to the pump motor. The test conditions, which were mainly orientated on reactor conditions, were varied over a wide range, in some cases also beyond reactor conditions. The power load and the fuel element position are related to the prototype reactor SNR 300. The experiments mainly served to ascertain the influence of the power tilt, the flow run-down characteristic, and a flatter radial temperature profile on the behavior during the flow transient. The test procedure was as follows: after the initial steady state conditions had been adjusted, the pump was switched off. The flow decreased according to the predetermined
characteristic. As a result, the temperatures increased in the bundle, boiling started, and the boiling region extended. The power was switched off by a safety system shortly after the onset of dryout.

In Japan experiments were carried out in the sodium boiling and fuel failure propagation test loop SIENA in the O-arai Engineering Center, PNC [8]. The bundle consisted of 37 electrically heated pins, with tantalum as the heating element. The electrical resistance of tantalum is changed by temperature such that the maximum heat flux boiling was higher than the mean axial heat flux by 10-20%. Each pin of 6.5 mm diameter was wrapped by a 1.3 mm diameter spacer wire with a 264.8 mm helical pitch. The diameters and the pin pitch of 7.9 mm had the same configuration as those in the MONJU fuel subassemblies. The bundle had a 450 mm heated length and a 715-mm simulated fission gas plenum at its downstream position. The 37 pins were installed in a hexagonal Inconel 600 tube of 10 mm thickness so that the gap between the peripheral pins and the inner duct wall was 1.45 mm. A thermal insulator on the outer wall of the hexagonal tube minimized heat losses through the duct wall. Chromel-alumel thermocouples of 0.3 mm in diameter were located at 10 cross-sections to measure pin surface and subchannel temperatures. They were embedded in the outer surface of the pin sheath, and the probes for subchannel temperature measurement were projected from the pin surface. Subchannel thermocouples also were
located in several spacer wires. The test section inlet and outlet temperatures and outer duct wall temperatures were monitored by additional thermocouples. Two types of void-meters were used, 14 resistive void-meters were attached within the test section. In addition, a total 10 Chen-type void-meters located in the spacer wires were used to detect local voids. The sodium velocities at the inlet and outlet of the test section were measured by electromagnetic flow-meters. Pressure transducers (frequency response range: 0-8300 Hz) also were provided at the inlet and outlet of the test section to measure the pressure changes during boiling. Output signals from the sensors were recorded using a digital data acquisition system controlled by a HP-2116C computer. Data were logged at the rate of 8000 points per second and stored on magnetic tapes for subsequent processing.

Transient boiling phenomena were investigated under loss-of-flow conditions. At first, the inlet temperature and the cover gas pressure were regulated and by-pass lines were closed to supply enough flow to the test section. The heat flux was held at a fixed value by adjusting the input power supplied to the heater pins. Thereafter, the flow to the test section was reduced exponentially by a control circuit. The onset of boiling was detected by the output signals from the pressure transducers and acoustic sensors, and was subsequently confirmed by signals from the flow-meters, void-meters, and thermocouples. 34 runs were conducted for the boiling experiments. The experimental conditions were:

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Initial flow velocity</td>
<td>3.54, 5.10-5.24 m/s</td>
</tr>
<tr>
<td>Inlet temperature</td>
<td>453-530°C</td>
</tr>
<tr>
<td>Time before inception of boiling</td>
<td>12-103 s</td>
</tr>
<tr>
<td>Flow velocity at inception of boiling</td>
<td>0.23-0.26, 0.67-0.77 m/s</td>
</tr>
<tr>
<td>Maximum axial heat flux in boiling</td>
<td>40.1-55.6, 80.3-97.5 W/cm²</td>
</tr>
<tr>
<td>System pressure at end of heated zone</td>
<td>1.21 x 10⁵ to 1.69 x 10⁵ Pa</td>
</tr>
</tbody>
</table>

Two-phase natural convection tests were performed in 37-pin bundle with total inlet blockage: ECONA experiments [9]. A wire wrapped bundle was put into two concentric hexagonal cans. The main features are as follows. Pin diameter is 8.5 mm heated length is 1000 mm, inner and outer hexcan flat-to-flat distance is 26 mm, and 77.4 mm, respectively, maximum pin power is 10 kW, maximum heat flux is 37 W/cm². Two bundles were used, with the same geometry but different axial power profile: the first bundle with uniformly heated pins, the axial power distribution of the second bundle was a chopped sinusoid.

In addition to the above-mentioned experiments a number of experiments has been performed with 19 and 61-pin test assemblies in THORS Facility at the Oak Ridge National Laboratory [10]. Temperatures of the sodium within the bundle have been as high as 1010°C, and cladding temperatures have been above the melting point of stainless steel (1400°C). Power levels in the test bundle of pin simulators have been as high as 36 kW/m for a total test bundle power of 1.3 MW. The facility has been in operation for about 30000 h with sodium circulation and for ~ 2500 h with varied power levels on fuel pin simulators. The sodium flow between the two hexcans simulates the leakage flow in the core between the subassemblies. Sixty wire thermocouples measure the sodium temperature at eight different levels in the bundle. The seven central pins are instrumented each with three thermocouples inside the cladding near the end of the heated length to detect dryout. Twenty thermocouples are embedded in the outer hexcan.

A large program on in-pile investigation of various accidents involving boiling phenomena were performed at the CABRI and SCARABEE facilities [11-15].
Although the research programs on liquid-metal boiling are directed to the safety of fast reactors, the research objectives are of a general nature. The main factors determining the boiling process are the flow regimes, heat transfer, and a pressure drop characteristic. These characteristics may be obtained in steady-state experiments. Therefore, the steady-state experiments can be regarded as basic investigations. Deep analysis of fundamentals of liquid metal boiling phenomena has been performed by Kottowski and Savatteri [7]. According to this paper, flow regimes observed in forced-convection liquid-metal stationary boiling are less complex than in water. In liquid-metals, the boiling phases relative to subcooled boiling and bubble boiling have no importance for technological processes. The dominating flow regimes are piston, slug, and annular flows. On the basis of the flow regimes observed during boiling, the separate flow model principle is the only one suitable for calculating the two-phase flow pressure drop.

Superheating investigation has led to a greater understanding of factors that influence the nucleation of boiling. Oxide impurities in sodium have a notable influence on nucleation. They consider that in technological plants working with liquid metal, superheating up to 150°C occurs. But many years of sodium boiling studies at Grenoble Research Center led them to conclude that superheat does not seem to be important when considering LMFBR safety applications, and this for the following reasons [6]: (a) superheat has never been observed in their slow transient sodium-liquid experiments. It has been observed for fast overpower transient experiments (up to 80°C) and had no important consequences on channel voiding, and (b) due to the fact that important radial-temperature differences may exist under bundle conditions (effects of geometry of heat sink), the superheat effects could be limited to a few subchannels and would have minor consequences on subassembly voiding. In loss-of-flow experiment [8] the measured maximum wall superheat was 36°C, and no effect of heat flux, temperature rise rate, and system pressure was observed.

Another important characteristic for evaluation of a sodium boiling process is boiling heat transfer. The following correlation for forced convection is recommended in [2]

\[
h = 0.204 \left[ L \left( X_n - 2X_1 \right) \right]^{0.7} P^{0.15} \left( G D_h/l \right)^{0.7} \text{[W/m}^2 \text{°C]},
\]

where

- \( L \) represents the latent heat of vaporization,
- \( G \) is mass flow per unit flow cross section,
- \( P \) is pressure,
- \( D_h \) is the hydraulic diameter,
- \( l \) is the heating length,
- \( X_n \) is the outlet vapor quality,
- \( X_1 \) is the subcooling inlet vapor quality (\( X_1 = C_p \Delta T/L \)).

Critical heat-flux measurements on forced convection with sodium have been published in [16, 17]. From typical measurements and from the relative evaluation of known measurements the following correlation has been developed [27]

\[
q = 0.216 L \left( 1 - 2X_1 \right) G^{0.807} \left( D_h/l \right)^{0.807} \text{[W/m}^2]\]

In [6, 18, 19] the following dryout correlation has been validated for forced and natural convection sodium boiling under steady-state or slow transients in single-channel and bundle geometry:

\[
q = 0.42 F_v s G L \left( 1 - X_d \right) D_h/l
\]
where
\[ F_v = \frac{94}{\text{Re}_v} \] for laminar flow,
\[ F_v = 0.316 \text{Re}^{0.25} \] for turbulent flow,
\( F_v \) - friction factor,
s is the slip ratio (Lockart-Martinelli),
\( \text{Re}_v = \frac{G D_t}{\mu_v} \),
\( \text{Re}_v \) - Reynolds number for the vapor flow,
\( X_d \) - dryout quality.

As observed in experiments, dryout at 1 kW per pin corresponds to a quality of about 0.8, dryout at 3 kW per pin corresponds to a quality of about 0.5 [6].

**Out-of-pile experiments performed on the CFNa and CESAR loops** at CEN-GRENoble have shown that sodium boiling develops in 3 periods (see Fig. 1.1.2.4) [19]. (1) **HOT SPOT BOILING**: Stable boiling bubbles appear at hot locations. This type of boiling only occurs at high heat flux when intra-subchannel temperature differences are significant. It may lead to early dryout but does not affect the mean sodium flow. (2) **LOCAL BOILING**: is defined when stable boiling spreads over several subchannels but not over the whole cross section of the bundle. Dryout onset during local boiling has been investigated in [16], and (3) **GENERALIZED BOILING** or "extended" boiling: is defined when stable boiling spreads over the whole bundle cross-section. Dryout is likely to occur.

The stability analysis for steady-state, slow transients and natural convection boiling in a subassembly relies mainly on the LEDINEGG stability criterion. This criterion is expressed for a stable flow by:
\[
d \frac{\Delta P_{\text{ext}}}{dQ} < \frac{\Delta P_{\text{int}}}{dQ}
\]
where: \( Q \) is the mass flow rate through the boiling subassembly, \( \Delta P_{\text{int}}(Q) \) is the pressure drop in the boiling subassembly as a function of \( Q \) (\( \Delta P_{\text{int}} \) is called the internal characteristic), \( \Delta P_{\text{ext}}(Q) \) is the pressure drop which is imposed by the external circuit when the flow through the subassembly is varied (\( \Delta P_{\text{ext}} \) is called the external characteristic). Figure 1.1.2.5 presents a picture of different situations which may be encountered for LOF or natural convection boiling analyses.

![Diagram](image1.png)

**Fig. 1.1.2.4. Stages of boiling in a 19-pin bundle during slow-flow transients**
In the experiments three main types of $\Delta P_{\text{int}}(Q)$ shapes [6] were observed (see Fig. 1.1.2.5). (1) at high power ($W > 8$ kW per pin), generalized boiling corresponds to an increase in the pressure drop when the mass flow rate decreases; thus, with a constant pressure heat ($\Delta P_{\text{ext}}$), generalized boiling would result in a flow instability, (2) at rather low power ($\sim 3 < W < 5$ kW per pin), the pressure drop begins to decrease at the onset of boiling. This behavior is followed by a new pressure-drop increase for lower mass flow rates. At a constant pressure head (approximately equal to single-phase elevation), this would mean stable natural-convection boiling, stabilized at the mass flow rate at boiling onset, if there are no important pressure losses at the bundle inlet. Throttling of the valve results in flow instability due to the fact that Ledineggs criterion is no longer satisfied, and (3) at very low power ($W < 3$ kW per pin), the pressure-drop curve ($\Delta P_{\text{ext}}$) decreases continuously when the inlet mass flow rate decreases. This means that a static-flow instability is no longer possible and that stable natural-convection boiling occurs at a mass flow rate that depends on inlet pressure losses (throttle valve, etc.).

The experiments for SUPER-PHENIX I loss-of-flow accident without scram show the following [19]. As the pumps have great mechanical inertia, the loss of flow is very slow and boiling conditions are reached about 10 min after the onset of the transient, when the power is between 1/4 and 1/3 of the nominal power level. According to the power level in the subassembly, onset of boiling can lead either to stable natural circulation boiling or to flow excursion. For low powers, the boiling zone extends very little into the fissile zone, and entirely over the upper breeder zone of the pins and the upper neutron shielding at the top of the subassembly. Due to the length of the upper neutron shielding (about 1 m) the spreading of the boiling zone induces very strong buoyancy effects: stable natural circulation boiling with low quality is calculated for power levels up to 10 kW per pin ($> 1.4$ nominal power level). There should be no dryout for these boiling conditions but rather a large reactor heat-up. For high powers, a flow excursion develops (i.e. a LEDINEGG instability), the subassembly flow is decreased to zero in about 15 s. This leads to dryout and consequent material melting.
Large account of information has been obtained during performing the transient boiling tests which simulated the unprotected loss of flow accident in the KNS 37-pin bundle [7].

The initial flow velocity was 3.4 m/s and the average pin power per unit length was 216 W/cm. Boiling started at 6.11 s at a relative mass flow rate of 25.2%. The first onset of dryout was detected at 9.25 s. At that time the mass flow had decreased to 3.8%. At 0.2s after the onset of dryout the electrical power was switched off. The course of boiling and the main physical events observed during boiling are shown in Fig. 1.1.2.6 (test L 22).

Fig. 1.1.2.6. Course of typical signals during boiling and extension of the boiling region in test L22
The experiments showed that the behavior during the flow transient was similar in all experiments independently from the parameter settings. This behavior can be described as follows: The expansion of the boiling region proceeded in two steps. The first step was characterized by a radial and axial growth of the boiling region. During this period an intensive mass and heat transfer took place from the boiling region to the subcooled edge zone of the bundle. The second period, characterized by only one-dimensional axial extension of the boiling region, started after the boiling front had reached the test section wall. The axial extension of the boiling region led to an increase in two-phase pressure drop and, hence, to an accelerated reduction of the mass flow down to flow reversal and dryout. Parameter variations showed the following results: the effect of a tilted power was present only during the three-dimensional expansion of the boiling region and was compensated by radial vapor flow. An extreme slow flow run-down caused the boiling region to grow preferably into the upper unheated part of the bundle. The flattening of the single phase radial temperature profile to simulate a radially more extended bundle caused the three-dimensional growth to become shorter and consequently the one-dimensional growth to become more dominant. Dryout always occurred during the one-dimensional expansion of the boiling region. The extension of the boiling region at the onset of dryout was not very different in all experiments even in the case of an extreme power tilt. Dryout spread nearly simultaneously over the entire heated part of the boiling region. When dryout occurred, only very low fractions of liquid remained in the boiling region. Aspects of boiling phenomena in subassemblies indicated by evaluation of experimental data have been theoretically confirmed by calculations of the reference loss-of-flow test (L22) done with the one-dimensional BLOW-3A code and the three-dimensional BACCHUS-3D/2P code. The dominantly one-dimensional characteristic of the boiling behavior after extension of the two-phase flow zone over the whole subassembly cross-section is supported by the relatively successful comparison of BLOW-3A results with experimental data. The measurement from the steady-state boiling experiments performed in this facility showed the large portion of the two-phase pressure drop due to spacer grids, which amounts, in terms of frictional losses, up to 60%. Cooling of the bundle was maintained up to average maximum vapor qualities of about 40%. Permanent dryout of the pins occurred at a quality of about 60%.

Other loss-of-flow experiments conducted in SIENA facility (37-pin bundle) enabled the following conclusions to be drawn concerning boiling phenomena [8]: (1) the bubble was formed at the end of heated zone first and then expanded mainly to the upstream central subchannels and to the downstream unheated zone according to the expansion of the saturated temperature region, (2) when the bubble covered the whole flow cross-section, the inlet flow reduction was accelerated, (3) the inlet and outlet flow velocities oscillated violently as the growth and collapse of a piston-like bubble, and an inlet flow reversal was observed, (4) dryout of pin surfaces occurred after the inception of flow reversal, and (5) dryout was once ceased by the rewetting, but it took place again in the next flow reversal. This sequence was repeated several times, so that gradually the dryout region expanded and the pin surface experienced higher temperatures. Data obtained from these experiments are being used in the computer model development for accident analysis and in its verification.

The transient boiling tests in THORS facility has been conducted in the 19- and 61-pin bundles to simulate a segment of a CRBR configuration fuel assembly [10]. Testing in 19-pin bundle confirmed the existence of a "boiling window", a locus of power-to-flow ratios in which boiling could occur for periods ranging from 20 to 90 s without clad dryout; although some of these runs might have ended in clad dryout had they been continued, the multidimensional character of sodium boiling behavior was clearly established by these tests. The most recent sodium boiling tests in 61-pin bundle have reinforced the conclusions drawn
from bundle data regarding the multidimensionality of sodium boiling. Several tests under natural-circulation conditions produced boiling for more than 11 min without dryout. In both bundles the occurrence of clad dryout was preceded by a static (Ledinegg) instability caused by the rapid generation of vapor and the resultant increase in two-phase pressure drop once the entire bundle cross-section was boiling. Other two-phase instabilities were seen, but no other appeared to affect significantly the overall system dryout behavior.

The sodium boiling experiments under natural convection conditions were carried out in the KNS 37-pin bundle in which the thermal and hydraulic characteristics of an SNR-300 subassembly were simulated [20]. In these experiments, which aimed to approach the limits of cooling of pin bundle, three aspects have been investigated: (a) increase of the flow resistance in the cold leg of the natural convection loop, (b) pin power exceeding the decay power, and (c) the maximum power which can be removed continuously from a grid spaced bundle with totally blocked inlet. The tests were divided into three groups according to their objectives. The main parameters of the tests are shown in Table 1.1.2.1 together with the objectives.

<table>
<thead>
<tr>
<th>Test group</th>
<th>Test parameters</th>
<th>Objectives</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>Pin power: 8 W/cm</td>
<td>Evidence for cooling also under boiling conditions</td>
</tr>
<tr>
<td></td>
<td>Without/with power tilt</td>
<td></td>
</tr>
<tr>
<td></td>
<td>Stepwise increased inlet flow resistance</td>
<td>Limit of cooling of the bundle</td>
</tr>
<tr>
<td>2</td>
<td>Constant inlet flow resistance</td>
<td>Margin of pin power in excess of decay power</td>
</tr>
<tr>
<td></td>
<td>Stepwise increased pin powers: 10-40 W/cm</td>
<td>Limit of flow stability</td>
</tr>
<tr>
<td>3</td>
<td>Totally blocked coolant inlet</td>
<td>Maximum power which can be removed continuously</td>
</tr>
</tbody>
</table>

In the tests conducted with pin powers corresponding to decay heat level (8 W/cm) and increased inlet flow resistances the limits of cooling were not reached within the conditions investigated. No permanent dryout was observed up to maximum vapor qualities of 24%. Due to the high degree of throttling which, in the case of maximum quality, corresponds to an inlet blockage of 97%, the flow is always stable. This could also be shown by means of one-dimensional two-phase calculations. The results reveal the large safety margin which exists between nominal operation conditions in the case of emergency cooling and cooling failure. For steady state conditions the two-phase flow showed a rather one-dimensional behavior at vapor qualities higher than about 6-8%. The application of a tilted power (+8%) had no noticeable effect, neither on the side-to-side temperature distribution in the heated length up nor on the vapor distribution across the flow cross-section. In the tests in which the flow resistance was kept at a moderate level pin powers of up to 30 W/cm (2700 W/pin) could be removed without loss of cooling. This value, which
was confirmed by the calculation, is much higher than that of the decay power. Flow instability occurred only at a higher power level. The maximum power which could be removed from the bundle blocked at the inlet was confined by the presence of the grids. It was found to be close to 270 W/pin (3 W/cm).

In the CABRI experiments [21] concerning an investigation of the behavior of fast reactor fuel pins in transient undercooling overpower conditions shown that a short time period of local boiling of up to 0.4 s occurred, which was initiated with a superheat in the bulk coolant flow of less than 1°C in most experiments and not exceeding 3°C even in exceptional cases. Its effect on the dynamic void extension phase is rather small. The transient evolution of the voiding zone is mainly controlled by the imposed linear rate and determined by evaporation and condensation processes. These are simulated accurately enough on the basis of two types of models, namely the multi-bubble slug ejection model and the homogeneous model. The dryout of the cladding begins around 0.55 to 0.65 m from the bottom of fissile column but fluctuations usually occur initially due to flow oscillations until a permanent dryout condition is established. However, a sodium liquid film on the structure wall remains after dryout of the cladding.

The SCARABEE program comprised a number of different types of in-pile tests aimed at achieving a better understanding of loss of coolant accidents. The tests were performed in 19 and 37 pin bundles using fresh and irradiated fuel pins [22-26]. The experiments related to sodium boiling phenomena included BE, BE+ and APL series. Two classes of accident covered in APL and BE+ test series were: (1) the global accident of a slow pump coastdown at nominal operating power, the APL series, and (2) the local accident of an instantaneous external blockage of a subassembly inlet under otherwise normal operating conditions, the BE+ series.

In APL series pump trip was simulated with halftime flow reduction of 20s. In the tests typical times for first dryout were 48 s (APL) and 3 s (BE+). All pump trip tests showed the same characteristics: a gradual reduction in flow level until boiling followed soon afterwards by a rapid reduction to a very low level. In each case, flow oscillations developed on both inlet and outlet, which were 180° out-of-phase, soon after boiling started. Therefore, it seems likely that they were reflecting a classical hydrodynamic instability situation where the two-phase component in oscillating pressure just compensates that arising in the single-phase region at inlet. The "instantaneous" inlet blockage tests also exhibited large oscillation on the outlet flow even after the inlet was essentially blocked. This somewhat surprising result is presumably due to a feed-back between the system pressure and evaporation/condensation in the upper structure. The main objective of the BE test [25] was to study the consequences of a total inlet blockage of a subassembly during reactor start-up, to demonstrate that this accident is less severe than a total instantaneous blockage of a subassembly at full power and that it can be detected by delayed neutrons before the hexagonal tube is damaged. Another objective was to provide data for natural convection in sodium, one or two phase, in view of codes validation. The bundle consisted of 37 pins of fresh and enriched UO₂ (fissile length: 0.6 m) within an hexagonal wrapper (4.6 mm thick). It was totally blocked at the inlet and cooled by an external sodium flow (0.135 kg/s or 0.270 kg/s) simulating the interassembly (I/A) gap flow. The inlet temperature was 450°C and the saturation temperature 950°C.

The steady state tests performed are summarized below (the test at boiling onset being shown in brackets).
The test chronology was the following:

<table>
<thead>
<tr>
<th>Time (s)</th>
<th>Max-flux (W/cm)</th>
<th>Event Description</th>
</tr>
</thead>
<tbody>
<tr>
<td>0</td>
<td>81</td>
<td>First clad dryout (central subchannel, top of fissile)</td>
</tr>
<tr>
<td>54</td>
<td>83.1</td>
<td>First clad melting (central subchannel, 10 cm below top of fissile)</td>
</tr>
<tr>
<td>100</td>
<td>80.1</td>
<td>First DND signal</td>
</tr>
<tr>
<td>495</td>
<td>101</td>
<td>Dryout of first wrapper and failure</td>
</tr>
<tr>
<td>507</td>
<td>102</td>
<td>Reactor trip</td>
</tr>
</tbody>
</table>

CONCLUSIONS

Many in-pile and out-of-pile experiments have been performed, up to now, in some research centers. Although the experiments were not always completely prototypical, the results obtained cover a wide parameter range of interest for reactor safety.

Three phases of sodium boiling in subassembly has been classified: hot-spot boiling, local boiling, and generalized boiling.

In general transient boiling behavior in the subassembly is governed by the three-dimensional effects and dryout does not follow immediately on boiling inception. The dryout flux correlation has been obtained and can be used in analytical models.

Some open problems are still existing: (1) boiling behavior for a subassembly with inlet completely blocked, submitted to decay heat and lateral cooling, and (2) yet there is no full understanding of the possible instability of boiling at a lower reactor power which would be a case of high importance for the safety analysis of reactors with advanced cores having zero void reactivity.

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1.1.3. Fuel pin and subassembly behaviour during accident conditions

For solving safety problems it is necessary to have information on fuel pin and subassembly behavior at all possible emergency situations. Such investigations are carried out all over the world and are aimed at studying a broad range of physical processes and obtaining, for example, such characteristics as:

- fuel pin failure thresholds,
- fuel and cladding material displacement, their repeat solidification and the secondary blockage of channels, including the effect on reactivity,
- fuel dispersion at an early stage of an accident,
- the effect of gaseous fission products on fuel failure and dispersion,
- molten fuel-coolant interaction effects,
- sodium boiling,
- heat transfer in the fuel-cladding-system,
- failure propagation from pin to pin,
- the influence of fuel pin and subassembly prehistory on accident consequences,
- postaccident heat removal, debris bed coolability,
- the behavior of large volumes of molten material.

The above physical processes and characteristics arise in such accidents as subassembly flow blockage; loss of flow (LOF), transient over power (TOP), loss of heat sink, and various combinations of these accidents. For the purpose of analysis these accidents can be accompanied by safety system action or be unprotected.

The most complete information can be obtained from in-pile experiments. For these purposes specialized single- or multipurpose- reactors are used.

According to their power level and duration of stationary conditions three classes of reactor facilities for safety tests can be noted:

- reactors capable of providing a given transient power variation without appreciable heat removal during tests,
- reactors allowing more durable (up to several minutes) operation at stationary power during a test and later on to realize the required transient power variations,
- reactors that ensure, in addition to conditions of shaped peaks and drops of power, extended pretest irradiation of experimental fuel elements and subassemblies under nominal prototype loads.

For pulsed operating conditions of such reactors it is necessary to ensure a transient ramp rate of up to 50 $/sec with its full insertion up to 5 dollars and energy release up to 10 kJ/g. For fast reactors a very important, reactivity-affecting mechanism is fuel relocation and voiding and thus research reactors should have hodoscope-type devices in their diagnostics system. Table 1.1.3.1 presents the research reactors on which most experiments on fast reactor safety are being conducted.

TREAT Experiments. Since the 1960s, a large program of experiments have been carried out at the TREAT reactor. Both fresh fuel elements and those irradiated in the EBR-II, PFR and FFTF reactors have undergone testing. The first experiments were carried out on metallic fuel elements modelling the EBR-II reactor fuel elements. Already in one of the first experiments [22] photographic recording of the fuel element melting process was
### TABLE 1.1.3.1. CHARACTERISTICS OF REACTORS FOR FUEL ELEMENTS TESTING UNDER TRANSIENT AND ACCIDENTAL CONDITIONS

<table>
<thead>
<tr>
<th></th>
<th></th>
<th></th>
<th></th>
<th></th>
<th></th>
<th></th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
<td>Power, Mw:</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>steady state</td>
<td>0.12</td>
<td>3.0</td>
<td>20 (10min)</td>
<td>94 (9min)</td>
<td>62.5</td>
<td>120</td>
<td>80-125</td>
</tr>
<tr>
<td>in-pulse</td>
<td>1.6 ( \times 10^4 )</td>
<td>5.2 ( \times 10^4 )</td>
<td>1.1 ( \times 10^4 )</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Cooling</td>
<td>air</td>
<td>water</td>
<td>water</td>
<td>water</td>
<td>sodium</td>
<td>water</td>
<td></td>
</tr>
<tr>
<td>Test conditions:</td>
<td>Capsules, loops</td>
<td>Capsules</td>
<td>Loops</td>
<td>Loops</td>
<td>two SAs in the core</td>
<td>SA in the core, a loop</td>
<td></td>
</tr>
<tr>
<td>arrangement type</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>number of pins</td>
<td>1-7</td>
<td>1-19</td>
<td>1-7</td>
<td>1-37</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Simulated conditions</td>
<td>LOF, TOP, TUCOP</td>
<td>TOP, LOF, PBE, PAHR</td>
<td>TOP, TUCOP</td>
<td>LOF, SA blockages</td>
<td>failed pin tests, transient conditions</td>
<td>LOF, TOP</td>
<td>Blockages</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
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</tr>
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<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
</tbody>
</table>
made. In experiments [23] a failure of metallic fuel already having been in contact with sodium was used. Later on experiments on irradiated fuel elements [24, 25] and in a system of 7 fuel elements [26] were started. To perform experiments on the melting of oxide fuel elements typical for FBRs, the MARK II loop with 7 fuel elements was designed. The description of the first experiments is given in [27]. In late 1960s a hodoscope was developed to observe fuel displacement. In [28] the test (L2) simulating the LOF accident is described. In [29] the results of experiments of the H-series are described, the aim of which was to study the fuel element behavior under conditions of accidents with a low ramp of 50 C/sec, to obtain information on the failure mechanism, failure criteria, fuel motion and the burnup effect. In [30] the experiments of the E-series (the E7 test) are described. Their main objective was to investigate some features of gas release from the cladding at its damage by melting fuel. A brief review of the series of experiments (H, E, L, R) is done in [31]. The tests of the F series [32] were aimed at obtaining information on fuel motion at overpower transient of 10 nominal power levels. In [33] a brief review of experiments carried out at the TREAT reactor within the frames of THE FUEL DYNAMICS PROGRAM at ANL during 9 years (till 1978) is given. All in all 14 experiments on modelling of fuel element behavior under hypothetical accident conditions were performed. In [34] a series of experiments (L) is described that was carried out for the purpose of studying fuel movement at a loss of coolant accident as applied to the FFTF and Clinch-River reactor conditions. The test conditions are presented below.

**TABLE 1.1.3.2. TEST CONDITIONS OF TREAT EXPERIMENTS**

<table>
<thead>
<tr>
<th>Parameter</th>
<th>L5</th>
<th>L6</th>
<th>L7</th>
<th>L8</th>
</tr>
</thead>
<tbody>
<tr>
<td>Linear load at pre- irradiation, kW/m</td>
<td>40</td>
<td>40</td>
<td>40</td>
<td>20</td>
</tr>
<tr>
<td>Burn-up, % h.a.</td>
<td>8</td>
<td>3</td>
<td>3</td>
<td>2</td>
</tr>
<tr>
<td>Transient conditions parameters:</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>initial linear load, kW/m</td>
<td>36</td>
<td>36</td>
<td>36</td>
<td>80</td>
</tr>
<tr>
<td>maximum linear load, kW/m</td>
<td>225</td>
<td>350</td>
<td>800</td>
<td>1520</td>
</tr>
<tr>
<td>Average period, ms</td>
<td>835</td>
<td>420</td>
<td>110</td>
<td>80</td>
</tr>
</tbody>
</table>

The experiments L5, L6, L7 were carried out with reducing sodium flow rate. On reaching the sodium boiling temperature a power excursion was realized (simulation of positive sodium void reactivity implication). Fuel element damage and fuel relocation took place in a drained subassembly, so no pressure rises were noted. In the experiment L8 an increase of power was carried out up to sodium boiling onset, as a result, short-duration pressure peaks up to 30 atm. were recorded. The measurements of the fuel worth in the course of experiments with the use of the hodoscope have revealed that its dispersion leading to a negative reactivity occurs.

Table 1.1.3.3 contains the results [35] from testing of FFTF irradiated fuel pins subsequently transient tested in TREAT. For the FFTF irradiated pins, only three pin failures were observed. In all three cases, the axial failure location was well above the axial midplane and substantial fuel sweepout was observed (no plugging of the coolant channel). Figure 1.1.3.1 indicates that the fuel pins will withstand over power conditions of at least three times full steady-state power prior to failure. For high ramp rates, the resistance to failure even better. Of particular interest is the degree of axial relocation of molten fuel inside the
<table>
<thead>
<tr>
<th>Test</th>
<th>Type</th>
<th>Test Date</th>
<th>Pin</th>
<th>Steady State Power (kW/M)</th>
<th>Burnup (Atom %)</th>
<th>Transient Rate f/s</th>
<th>Failure Location (Fraction of Height)</th>
<th>Pin Failure Mechanism</th>
<th>Post-Failure Conditions</th>
</tr>
</thead>
<tbody>
<tr>
<td>RFT-L1</td>
<td>TOP</td>
<td>(8/82)</td>
<td>1</td>
<td>41</td>
<td>0.2</td>
<td>50</td>
<td>----</td>
<td>----</td>
<td>----</td>
</tr>
<tr>
<td></td>
<td>&quot;</td>
<td></td>
<td>2</td>
<td>41</td>
<td>0.2</td>
<td>50</td>
<td>----</td>
<td>----</td>
<td>----</td>
</tr>
<tr>
<td></td>
<td>&quot;</td>
<td></td>
<td>3</td>
<td>29</td>
<td>0.2</td>
<td>50</td>
<td>(Top 1/3)</td>
<td>(internal)</td>
<td>(dispersive)</td>
</tr>
<tr>
<td>RFT-L2</td>
<td>TOP</td>
<td>(6/83)</td>
<td>1</td>
<td>28</td>
<td>0.2</td>
<td>5</td>
<td>----</td>
<td>----</td>
<td>----</td>
</tr>
<tr>
<td></td>
<td>&quot;</td>
<td></td>
<td>2</td>
<td>41</td>
<td>0.2</td>
<td>5</td>
<td>----</td>
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</tr>
<tr>
<td></td>
<td>&quot;</td>
<td></td>
<td>3</td>
<td>47</td>
<td>0.2</td>
<td>5</td>
<td>----</td>
<td>----</td>
<td>----</td>
</tr>
<tr>
<td>RFT-L3</td>
<td>TOP</td>
<td>(1/84)</td>
<td>1</td>
<td>35</td>
<td>2.6</td>
<td>5</td>
<td>----</td>
<td>----</td>
<td>----</td>
</tr>
<tr>
<td></td>
<td>&quot;</td>
<td></td>
<td>2</td>
<td>36</td>
<td>5.2</td>
<td>5</td>
<td>----</td>
<td>----</td>
<td>----</td>
</tr>
<tr>
<td></td>
<td>&quot;</td>
<td></td>
<td>3</td>
<td>38</td>
<td>5.2</td>
<td>5</td>
<td>----</td>
<td>----</td>
<td>----</td>
</tr>
<tr>
<td>RFT-L4</td>
<td>TOP</td>
<td>(2/84)</td>
<td>1</td>
<td>35</td>
<td>2.6</td>
<td>100</td>
<td>----</td>
<td>----</td>
<td>----</td>
</tr>
<tr>
<td></td>
<td>&quot;</td>
<td></td>
<td>2</td>
<td>36</td>
<td>5.4</td>
<td>100</td>
<td>----</td>
<td>----</td>
<td>----</td>
</tr>
<tr>
<td></td>
<td>&quot;</td>
<td></td>
<td>3</td>
<td>38</td>
<td>5.3</td>
<td>100</td>
<td>----</td>
<td>----</td>
<td>----</td>
</tr>
<tr>
<td>TS-1</td>
<td>TOP</td>
<td>(8/83)</td>
<td>1</td>
<td>41</td>
<td>0.2</td>
<td>5</td>
<td>(Top 1/3)</td>
<td>(internal)</td>
<td>(dispersive)</td>
</tr>
<tr>
<td>TS-2</td>
<td>TOP</td>
<td>(1/84)</td>
<td>2</td>
<td>36</td>
<td>5.8</td>
<td>5</td>
<td>(Top 1/3)</td>
<td>(internal)</td>
<td>(dispersive)</td>
</tr>
</tbody>
</table>
fuel pin for TS-1 and TS-2. Such movement has significant negative reactivity implications. If substantial numbers of pins were to behave in such a manner during an actual TOP event, and, if the in-pin fuel motion occurred sufficiently quickly, it is possible that sufficient negative reactivity would occur that the reactor power would drop and fuel pin failure would be prevented entirely. This phenomenon is expected to be enhanced for annular pins (designs containing a fabricated central void region) - especially if they contain annular blankets.
In November 1979, a US/UK agreement was reached to irradiate pins in PFR and then subject them to accident simulations in TREAT (the PFR/TREAT program). Table 1.1.3.4 [35, 36] contains the principal results from this test series. The test series included both fast and slow TOP simulations and several transient undercooling overpower (TUCOP) simulations. The latter tests were directed to study fuel pin behavior for loss of cooling events which could cause a subsequent overpower excursion due to positive reactivity from sodium voiding. Both single pin (CO series) and 7 pin (LO series) experiments were included, using pins having a range of burnups from fresh to goal exposure. As can be seen from the table, the preponderance of pin failure data for TOP specimens tested in a flowing sodium loop indicated failure locations well above the axial midplane. Post-test examinations generally indicate rather localized cladding failures - rather than long rips. This tendency for localized failures was confirmed by a series of out-of-pile rip propagation tests conducted at HELD [38]. However, the present U.S. data base does not include situations in which internal pressure is maintained within the pin, which could cause failure extension. Four pin failure mechanisms were identified to be operable within the PFR/TREAT test series: 1) internal pressurization, 2) fuel cladding mechanical interaction, (3) molten fuel contact with the cladding, and (4) cladding melting (which only occurs for dried-out coolant channel conditions, e.g. TUCOP).

There was evidence of pre-test axial fuel motion in some of the tests and in most cases there was substantial fuel dispersal and significant fuel sweepout subsequent to failure. Flow blockage tended to be more prevalent for fresh fuel (e.g. LO1), where the lack of fission gas pressure allowed a high fraction of molten fuel to be generated prior to pin (failure), or high ramp rates (e.g. LO2). In all cases there was sufficient fuel movement and dispersal such that a substantial decrease in fuel worth would have resulted if such failure had occurred within a whole core environment. In [39] the tests of fuel elements in TREAT during the 1980s are summarized. In addition to the above tests with the FFTF fuel elements, there were tests performed on advanced CDE type fuel elements featured larger diameter (0.686 mm) (the series CDT). In the series PFT and TS the fuel cladding material 20% GW 316 SS was used, and in the CDT series-HT9 the burn-up was extended up to 12.5%.

In the CDT tests, the FFTF-irradiated fuel pins sustained 5 c/s overpower transients in TREAT of up to 4.5 nominal FFTF power levels without failure, and 1 $/s transient ramp rates were conducted to overpower levels of over sixteen times nominal power before fuel pin rupture. These long lifetime fuel pins also demonstrated a propensity for pre-failure axial fuel relocation, a highly desirable inherent safety characteristic which can serve to mitigate or, potentially, terminate transient overpower accidents. The failed pins exhibited breaches attributable to cladding melt-through just above the fuel column mid-plane. The transient overpower performance capability of both CDE and the reference fuel design are presented in Figure 1.1.3.1. The CDT-1 and CDT-3 experiments provided the opportunity to compare the relative transient performance of solid versus annular fuel pin design. The two fuel pins, while having different burnup levels (CDT-1 - 12.5 at% and CDT-3 - 6.3 at%), were subjected to similar 5 c/s overpower transients. Post-test evaluations suggested that the maximum fuel enthalpy reached in the CDT-3 pin with annular fuel was over 20% greater than the CDT-1 with solid fuel. In spite of the higher power level and higher cladding temperature, the annular fuel pin showed less transient-induced strain than did the solid pin: 1.4% versus 2.5%. Results from the experiment suggest that the central hole in the annular design serves as an effective pathway to the plenum, thus alleviating pressure buildup in the fueled region during overpower transients.
TABLE 1.1.3.4. FUEL PIN TRANSIENT TEST RESULTS FROM THE PFR/TREAT PROGRAM

<table>
<thead>
<tr>
<th>Test</th>
<th>Type</th>
<th>Test Date</th>
<th>Pin Type</th>
<th>Burnup (Atom %)</th>
<th>Transient Rate (ft/s)</th>
<th>Failure Location (Fraction of Height)</th>
<th>Failure Mechanism</th>
<th>Post Failure Condition</th>
</tr>
</thead>
<tbody>
<tr>
<td>C01</td>
<td>TOP</td>
<td>11/80</td>
<td>1 pin capsule</td>
<td>Fresh</td>
<td>500</td>
<td>0.50 / 0.25</td>
<td>molten fuel</td>
<td>----</td>
</tr>
<tr>
<td>C02</td>
<td>TOP</td>
<td>11/81</td>
<td>1 pin capsule</td>
<td>4</td>
<td>500</td>
<td>0.70</td>
<td>internal pressure</td>
<td>----</td>
</tr>
<tr>
<td>C03</td>
<td>TOP</td>
<td>12/81</td>
<td>1 pin capsule</td>
<td>9</td>
<td>500</td>
<td>0.25</td>
<td>FCMF</td>
<td>----</td>
</tr>
<tr>
<td>C04</td>
<td>TOP</td>
<td>7/82</td>
<td>1 pin loop</td>
<td>4</td>
<td>5</td>
<td>0.95</td>
<td>internal pressure</td>
<td>dispersive</td>
</tr>
<tr>
<td>C05</td>
<td>TOP</td>
<td>12/82</td>
<td>1 pin loop</td>
<td>9</td>
<td>5</td>
<td>0.95</td>
<td>internal pressure</td>
<td>dispersive</td>
</tr>
<tr>
<td>C06R</td>
<td>TUCOP</td>
<td>7/83</td>
<td>1 pin loop</td>
<td>4</td>
<td>---</td>
<td>0.80</td>
<td>FCMF</td>
<td>dispersive</td>
</tr>
<tr>
<td>L01</td>
<td>TOP</td>
<td>11/80</td>
<td>7 pin loop</td>
<td>Fresh</td>
<td>500</td>
<td>0.55</td>
<td>molten fuel</td>
<td>fuel movement</td>
</tr>
<tr>
<td>L02</td>
<td>TOP</td>
<td>3/82</td>
<td>7 pin loop</td>
<td>4</td>
<td>500</td>
<td>0.55</td>
<td>internal pressure</td>
<td>flow blockage</td>
</tr>
<tr>
<td>L03</td>
<td>TOP</td>
<td>5/82</td>
<td>7 pin loop</td>
<td>4</td>
<td>5</td>
<td>0.85</td>
<td>internal pressure</td>
<td>significant dispersal</td>
</tr>
<tr>
<td>L04</td>
<td>TUCOP</td>
<td>2/83</td>
<td>7 pin loop</td>
<td>4</td>
<td>---</td>
<td>0.65</td>
<td>clad melt</td>
<td>very dispersive</td>
</tr>
<tr>
<td>L05</td>
<td>TUCOP</td>
<td>4/83</td>
<td>7 pin loop</td>
<td>4</td>
<td>---</td>
<td>0.50</td>
<td>FCMF</td>
<td>dispersive</td>
</tr>
<tr>
<td>L06</td>
<td>TUCOP</td>
<td>9/83</td>
<td>7 pin loop</td>
<td>Fresh</td>
<td>---</td>
<td>0.70</td>
<td>clad melt</td>
<td>dispersive</td>
</tr>
<tr>
<td>L07</td>
<td>TUCOP</td>
<td>12/83</td>
<td>7 pin loop</td>
<td>4</td>
<td>---</td>
<td>0.50</td>
<td>internal pressure</td>
<td>molten fuel</td>
</tr>
</tbody>
</table>
In the M-series tests of metallic fuel behavior during TOP conditions were carried out (M-series). Testing of metallic fuels resumed in 1984 under the Integral Fast Reactor (IFR) Program at Argonne National Laboratory. Advances in metal-fuel design since the 1960s had allowed for higher swelling, higher burnup, and greater margin to failure, thereby making much of the database from the earlier TREAT tests obsolete. After a preliminary test M1 on a small fuel sample in a dry capsule, six tests (M2 through M7) on modern metallic fuel were performed in Mark-III sodium loops. All six investigated the response of the fuel to anticipated transient without scram (ATWS) transient overpower conditions, in particular the fuel failure mechanisms, prefailure fuel expansion, and early post-failure fuel movement. The parameters in the tests were fuel composition, burnup, and fuel conditions at test termination, as indicated in Table 1.1.3.5.

<table>
<thead>
<tr>
<th>Test</th>
<th>Fuel Composition</th>
<th>Burnup (ha.%</th>
<th>Post test Condition</th>
</tr>
</thead>
<tbody>
<tr>
<td>M2</td>
<td>U-5Fs</td>
<td>0.3</td>
<td>intact</td>
</tr>
<tr>
<td></td>
<td></td>
<td>4.4</td>
<td>failed</td>
</tr>
<tr>
<td></td>
<td></td>
<td>7.9</td>
<td>failed</td>
</tr>
<tr>
<td>M3</td>
<td>U-5Fs</td>
<td>0.3</td>
<td>intact</td>
</tr>
<tr>
<td></td>
<td></td>
<td>4.4</td>
<td>intact</td>
</tr>
<tr>
<td></td>
<td></td>
<td>7.9</td>
<td>intact</td>
</tr>
<tr>
<td>M4</td>
<td>U-5Fs</td>
<td>fresh</td>
<td>intact</td>
</tr>
<tr>
<td></td>
<td></td>
<td>2.4</td>
<td>failed</td>
</tr>
<tr>
<td></td>
<td></td>
<td>4.4</td>
<td>intact</td>
</tr>
<tr>
<td>M5</td>
<td>U-19Pu-10Zr</td>
<td>0.8</td>
<td>intact</td>
</tr>
<tr>
<td></td>
<td></td>
<td>1.9</td>
<td>intact</td>
</tr>
<tr>
<td>M6</td>
<td>U-19Pu-10Zr</td>
<td>1.9</td>
<td>intact</td>
</tr>
<tr>
<td></td>
<td></td>
<td>5.3</td>
<td>failed</td>
</tr>
<tr>
<td>M7</td>
<td>U-19Pu-10Zr</td>
<td>9.8</td>
<td>failed</td>
</tr>
<tr>
<td></td>
<td>U-10Zr</td>
<td>2.9</td>
<td>intact</td>
</tr>
</tbody>
</table>

Note: Cladding was 316SS for U-5Fs fuel pins, D9 for U-19Pu-10Zr pins, and HT9 for the U-10Zr pin.

Until irradiated fuel pins of IFR reference fuel (U-Pu-Zr alloy) were available, the fuel that was the EBR-II driver fuel in the early 1980s (U-5 wt.% fissium) was tested. (Fissium is a mixture of metals representing an equilibrium composition of solid fission products that would result using a simple pyrometallurgical fuel cycle). In all of the six loop tests, each of the fuel pins was located in a separate flowtube and an overpower transient with 8 s period exponential rise was used. Data from the tests were instrumental in developing models of cladding failure and prefailure fuel elongation. In the relatively-short time frame of the tests, cladding failure was caused by a combination of pin plenum pressure acting hydrostatically through weak partially-molten fuel on cladding that had been thinned by transient-induced formation of a low-melting-temperature fuel-cladding alloy. The relative importance of pressure and cladding thinning was found to be a strong function of burnup. At low burnups, it was necessary for peak cladding temperatures to reach a value at which the rate of metallurgical attack of the cladding increased sharply by two or three orders of magnitude. In general, cladding failure is a function of time and temperature. For the heating conditions applied in these tests, failure of both U-5Fs and U-19Pu-10Zr fuel in austenitic cladding
consistently occurred at conditions corresponding to 4.0-4.4 times IFR reference operating power. The failure threshold for HT9-clad U-10Zr fuel was not measured but was found to exceed 4.8 times nominal conditions. Cladding failure consistently occurred at the very top of the fuel column and was very localized. Nearly all of the molten-alloy (roughly half of the fuel) that was present in the pin at the time of failure was expelled through that small breach and was carried upward out of the core region by the flowing sodium. The non-molten alloy at the bottom of the fuel pin remained in place. Although the test geometry did not well represent a pin-bundle configuration, the observed propensity of the fuel to monotonically disperse in a manner that would be a strong advantage in mitigating or preventing hypothetical severe accidents can be considered to be a basic characteristic of metal fuel.

Prefailure fuel elongation was found to be sensitive to fuel composition and preirradiation power level. In addition, where those parameters led to potentially high elongations, there was strong dependence on burnup. The U-5Fs fuel irradiated at 26 kW/m expanded up to 17% axially at low burnups, but the reference U-Pu-Zr fuel expanded during the tests by only 2-4% and nearly independently of burnup. Models that were developed to predict the transient-induced elongation were quite simple and accurate. In-pile TOP testing of LMFBR advanced, carbide-type fuel was done between 1974 and 1982. Fresh and EBR-II irradiated carbide fuel was tested in the TREAT facility. The test vehicle used a single, EBR-II type pin in stagnant sodium. Over a dozen such tests have been done. The results of the latest are summarized in Table 1.1.3.6 [40]. These tests have shown sodium and helium bonded, 7.9 and 9.4 mm diameter carbide pin designs to have satisfactory transient operational capability. Reactivity ramp rates from $0.5/s$ to $3/s$ to 300% over design power operation have been studied. Margins to breaching have been shown to equal or exceed those of the oxide. State-of-the-art advanced fuel pin performance codes accurately predict performance in the range of conditions investigated.

**ACRR Experiments.** In [41] there is a brief description of the PBE-series experiments at SANDIA Lab. carried out in the Annular Core Research Reactor (ACRR) with the aim to determine the scales of pressure pulses and the thermal-to-mechanical energy conversion ratio of severe accidents. Specific energy releases in a fuel element are 2.35-2.9 kJ/g at a pulse half-width ~5 ms. In [42] the description of upgrading the ACRR reactor is presented, which allows the investigations of fuel pins under conditions of severe accidents: TOP, LOF, PBE. Estimates reveal a possibility of a specific energy of release ~4 kJ/g with a total energy release of 200 mj. A series of fuel disruption experiments simulating LOF accident transient for homogeneous-core LMFBRs is currently being performed in the ACRR [43]. The test fuel is observed with high-speed cinematography to determine the timing and the mode of the fuel disruption. The five experiments performed to date shown that the timing and mode of fuel disruption depend on the power level, fuel temperature (after preheat and at disruption), and the fuel temperature gradient. Two basic modes of fuel disruption were observed; solid-state disruption and liquid-state swelling followed by slumping. Solid-state dispersive fuel behavior (several hundred degrees prior to fuel melting) is only observed at high power levels ($6P_0$), low preheat temperatures ($2000$ K), and high thermal gradients ($2800$ K/mm). The swelling/slumping behavior was observed in all cases near the time of fuel melting.

The in-pile CABRI test programme initiated in 1973 is jointly performed by CEA, KFK, PNC and UKAEA. The first series, now called CABRI-I, consisting of 32 tests simulating TOP, TUCOP, and ULOF conditions in fast sodium-cooled breeder reactors has been successfully terminated in 1987. A new series, CABRI-II, using different claddings and pellet designs and higher burnup, as well as slower ramp rates, has recently been conducted. At that time only the TREAT programme was under way using LMFBR-specific fuel pins of
TABLE 1.1.3.6. SUMMARY OF TREAT TEST OF ADVANCED, CARBIDE-TYPE FUEL

| Purpose       | HC1     | SC1     | SC-1A    | HC2     | SC3     | HC3     | SC4     | HC4     | HC5     | HC-4A   |
|---------------|---------|---------|----------|---------|---------|---------|---------|---------|---------|---------|---------|
|               | Calibration | Integrity | Bond behavior | Integrity | Breaching | Breaching | Integrity | PPS Term | Integrity |         |
| Fuel Pin      | K6B     | AIR-I | AIR-I     | K6B-86  | AIR-1-100| K6B-84  | K7-6     | WSA-32-28| WSA-32-18| K10-02  |
| Pin Diameter (mm) | 7.87    | 7.87   | 7.87     | 7.87    | 7.87    | 7.87    | 7.87    | 9.40    | 9.40    | 9.40    |
| Burnup (at.%) | 0       | 0      | 6.3      | 8.4     | 6.2     | 8.3     | 8.4     | 12      | 8.3     |         |
|                | ~60C/s  | ~ 80 C/s | ~ 70 C/s | 50 C/s  | 50 C/s  | 50 C/s  | 50 C/s  | 50 C/s  | 35 C/s  | 50 C/s  |
| Thermally Simulated FFTF Ramp |         |         |          |         |         |         |         |         |         |         |
| Peak Pin Power in TREAT (kW/m) | 120     | 250    | 384      | 164     | 148     | 249     | 245     | 223     | 352     | 243     |
| Peak Fuel Temperature (°C)     | 1650    | 1600   | 2480     | 1600    | 1350    | 2300    | 2850    | 1840    | 1710    | 2090    |
| Peak Midwall Cladding Temperature (°C) | 650    | 870    | 990      | 720     | 700     | 970     | 980     | 770     | 576     | 835     |
| Cladding Integrity | Intact | Intact | Intact   | Intact  | Intact  | Breached| Intact  | Intact  | Intact  | Intact  | Breached|
| Cladding Damage | No      | No     | Ovality  | dD/D= 0.10% | No   | Longitudinal cracks | dD/D= 0.36% | dD/D= 0.14% | dD/D= 0.10% | NA     |
| Fuel Damage    | Mild cracking | No       | Cracking separation | No | No | Cracking swelling | Cracking swelling | Minimal | Minimal | Cracking |


US-design for testing. Extrapolation to SNR-300 or PHENIX-type fuel pins was difficult and only very little experience with irradiated fuel was available. Moreover, the TREAT design did not allow sufficiently fast power pulses which were considered necessary for testing under severe transient conditions, nor could TREAT be operated with steady state conditions representative for LMFBR conditions. The information obtained from the TREAT programme and its influence on the development of theoretical models to describe the effects of HCDAs was of high significance, but it was far from being complete to solve all problems which were anticipated to come up in the course of the licensing procedures of future large LMFBRs. Therefore it was generally recognized that an additional effort complementing the TREAT programme had to be attempted.

The main objective of the CABRI test program was to arrive at a better phenomenological understanding of the fast transient domain of initiation phase scenarios considered in risk analysis studies by observation of the behavior of a single pin subjected to transient conditions in the test vehicle [44]. The transient energy deposition in the test fuel is varied according to the test objectives between values of 0.5 kJ/g (partial fuel melting without cladding failure) through intermediate values of 1 kJ/g (extensive fuel melting leading to clad failure and release of molten fuel into the coolant channel) up to a maximum value of about 2 kJ/g (fuel vaporization). The thermal hydraulic conditions in the coolant channel are varied to obtain to pin failure under single-phase or two-phase conditions with the clad in solid or molten state. The test sequence allows to determine the cladding failure threshold and the conditions of fuel/clad interactions and to assess the effect of fuel and steel vaporization on the release and movement of molten fuel.

The CABRI-I test programme was divided into two series of test corresponding to the two accident types investigated: (1) tests consisting of a fast TOP initiated from typical reactor steady state conditions. These are the so-called A-type tests, and (2) tests with sodium flow rate reduction initiated from nominal steady state conditions to simulate reactor conditions during an unprotected pump coast down. These tests are usually designated as TUCOP tests. There are also flow reduction tests without TOP, so-called unprotected loss-of-flow (ULOФ) tests, terminated by a reactor scram, serving as reference tests for the TUCOP tests. These are the so-called B-type tests.

The transient overpower is usually of a small half-width between 10 and 40 ms. It is initiated by the fast opening of a large-aperture valve, which releases He gas from the core region, thereby rapidly increasing the core reactivity and hence power. These extremely fast excursions were assumed at the time of the programme definition to be the initiator of severe disassembly accidents. The outcome of safety-related investigations and experiments - including CABRI-I - have shown in the meantime the very small likelihood of rapid power transients. Therefore, in the last phase of the test programme the emphasis was shifted to the study of phenomena occurring during slower reactivity ramps. In these tests a power trace is applied consisting of a propels of about 500ms followed by the main pulse with a half-width of about 40 ms. These are so-called structured transients which are generated by depressurization through a small-aperture valve followed by a deferred opening of a large-aperture valve. All tests were performed with one type of test pin, the Common Test Pin, whose design with small tolerances, inspections of pellets and tubes under severe quality control requirements ensured that all pins of the same batch could be regarded as identical. This fundamental choice has greatly enhanced the deduction of general conclusions from the test programme. The test fuel consisted of pellets of 20%-enriched UO$_2$ for the fresh fuel tests, and 15.5% Pu/(Pu+U) in the form of mixed oxide (U,Pu)O$_2$ for tests with irradiated fuel. The fuel was clad with 316-20% c.w. stainless steel. In order to study the influence of
burnup on cladding failure and the impact of fission products on fuel motion, the mixed oxide fuel pins were preirradiated in PHENIX to a burnup of 1at.% (1-type) and 5at.% (H-type) and in PFR to 3at% (G-type). The axial fuel expansion could be observed in all tests. It is greatest in experiments with fresh fuel pins and with open fuel/clad gaps, but it is also significant for ULOF tests where clad heating leads to gap re-opening and where considerable fuel expansion was observed after clad melting. The presence of an otherwise undesirable, strong neutron self-shielding effect even permitted the observation of fuel swelling and break-up. Two pin destruction modes could be discerned: (1) a very fast and localized fuel ejection in the case of mechanical clad failure, and (2) a slower, axially extended increase of the fuel pin diameter due to swelling and/or break-up in tests with thermally weakened or molten clad.

The fuel motion in the coolant channel can be separated into 2 phases: a preferentially upward directed fuel motion during the first 50 ms, followed by fuel accumulations, generally 20 to 40 cm apart from the initial failure location and which remain essentially in place in the further progress of the transient.

The CABRI-II Project was in charge of an experimental programme of 12 in-pile tests [45]. The general objective of the programme was to study the behavior of irradiated fast reactor fuel pins under different power and/or flow rate transient conditions. The investigations focused on: (a) fuel pin behavior up to failure and failure threshold, (b) material relocation and blockage formation, (c) low energy injection rates, increasing the period of energy deposition compared to CABRI-I, and (d) margin to failure in slow ramp tests, linked to a control rod withdrawal in the reactor case.

The points a) and b) have been explored in the CABRI-I programme using test pins of single geometry, with low or medium burn-up. The objective of the CABRI-II programme is to extend the validation of code models to test pins with high burn-up; annular fuel, compared to "solid" fuel (no fabricated central hole in the pellets); new clad material.

The influence of these new parameters will also be tested with new types of transients, as announced above in points c) and d). The 12 tests of the matrix are presented in Table 1.1.3.7 [45].

The CABRI-II tests were performed smoothly with regard to instrumentation behavior and experimental conditions. The results obtained confirm the necessity for the investigation of the new parameters. Preliminary results of the interpretations study have provided valuable information on key phenomena and, in particular, strengthened the essential part of the CABRI-I outcome. They are summarized as follows: (1) the annular fuel design itself does not change the major transient fuel responses experienced in the CABRI-I solid fuel tests under the high power transient (> 10 kJ/g/s), and the knowledge integrated in CABRI-I can be applicable for the annular fuel design, (2) the non-failure result of E4 suggests a high failure resistance for the high burn-up fuel pin with a high fuel smear density and a still ductile clad material, (3) the large fuel expansion in E4 clearly demonstrates that fuel expansion is effectively operational in a wide burn-up range over 10 at.%, and (4) the extended fuel relocation observed in E7 and E8 is dominated by the failure and cooling conditions, and these test results support the CABRI-I interpretation on the fuel relocation mechanism.

BR2 experiments. In the frame of the European Collaboration on fuels and materials, the fuel safety needs were examined taking into account current core materials and designs. Some experiments have been performed in sodium cooled in-pile capsules in the Petten HFR
### Table 1.1.3.7. The CABRI-2 Test Matrix

<table>
<thead>
<tr>
<th>Test number</th>
<th>Type of power transient</th>
<th>Pin</th>
<th>Comments</th>
</tr>
</thead>
<tbody>
<tr>
<td>E5</td>
<td>fast structured</td>
<td>Ophelie-6</td>
<td>Non failure test for thermal calibration</td>
</tr>
<tr>
<td>E7</td>
<td>fast structured</td>
<td>Viggen-4</td>
<td>Failure at nominal flow rate</td>
</tr>
<tr>
<td>E8</td>
<td>fast structured</td>
<td>Viggen-4</td>
<td>Failure in a semi-restrained channel</td>
</tr>
<tr>
<td>E4</td>
<td>medium</td>
<td>Viggen-4</td>
<td>Non failure test for thermal calibration</td>
</tr>
<tr>
<td>E6</td>
<td>medium</td>
<td>Viggen-4</td>
<td>Failure at nominal flow rate</td>
</tr>
<tr>
<td>E2</td>
<td>medium</td>
<td>Viggen-4</td>
<td>Failure in a restrained channel</td>
</tr>
<tr>
<td>E3</td>
<td>medium</td>
<td>Viggen-4</td>
<td>Failure in a semi-restrained channel</td>
</tr>
<tr>
<td>E13</td>
<td>medium</td>
<td>Viggen-4</td>
<td>Failure in unrestrained channel</td>
</tr>
<tr>
<td>E12</td>
<td>slow ramp</td>
<td>Viggen-4</td>
<td>Linear ramp (1% Pn/s) up to failure</td>
</tr>
<tr>
<td>E9</td>
<td>slow ramp</td>
<td>Ophelie-6</td>
<td>Linear ramp (1% Pn/s) up to failure</td>
</tr>
<tr>
<td>E9 bis</td>
<td>slow ramp</td>
<td>Viggen-4</td>
<td>Study of failure at 20% melt fraction</td>
</tr>
<tr>
<td>E11</td>
<td></td>
<td>Viggen-4</td>
<td>Double power peak simulating aspects of the early transition phase</td>
</tr>
</tbody>
</table>

and in the VIC loop in the BR 2 reactor [46]. In the experiment Mol-18-B3, a KNK fuel pin (7.6 mm outer diameter) was pre-irradiated in steady state conditions up to 2.4 at% BU. After this period, 3 transient phases were performed, the first and second from 510 W/cm to about 780 W/cm within 16 seconds and the third as a combined power/temperature transient up to 640 W/cm and 900°C sodium outlet temperature. During the power transient a maximum rating of about 780 W/cm was reached while the temperature transient saw 900°C as maximum. Post-irradiation examination showed that despite the severe operational conditions, no clad deformation had been produced. Fuel structure was, as expected, with the fuel/clad gap completely closes.

A RELIEF in-pile rig, accommodating a special manufactured test fuel rod, equipped with a double bellows was designed for use in the Pool Side Facility of HFR. The RELIEF experiments are to provide information about the effective differential axial expansions. The first RELIEF-10 test was conducted to test the system, involving power transients from 80% to 100%, 115% up to 135% of the nominal power of 500 W/cm. The measured data proved the smooth functioning of the measuring system. The second RELIEF-11 experiment was irradiated at power jumps from 100% to 115%, 125%, 135% up to 160% of the nominal power to simulate power transients caused by rapid control rod changes and control rod ejection, respectively; all measured data were recorded. After a total of 100 in-pile days, the inner bellows developed a leak. The RELIEF-12 experiment is under way; power transients from 100% to 120%, 140% and 160% of the nominal power and the associated increases in cladding temperature by 30, 50, and 120 K are attained. This is to be a simulation of a case of excessive control rod movements with short and long recovery periods. The next experiment, RELIEF-13, simulate the pump failure incident at various power levels and under power decrease conditions, respectively, with power levels ranging between 30% and 100%.
and a possibility to raise the cladding temperature up to 150K by controlling the gas mixtures in the heat transfer pipes of the RELIEF capsule.

The main results obtained from this transient programmes are: (1) during over power conditions, fuel melting was experienced but fuel pin integrity was not affected, (2) peak clad temperatures up to 900°C were sustained for short times without failure, and (3) the fuel pin construction is sufficiently robust to survive severe transient conditions at burn-up levels to - 10%.

**SCARABEE experiments.** The SCARABEE test reactor has been used to bring fast breeder core materials to melting, even to boiling, with pin bundles or pools or geometries specially designed for the study of fuel melt propagation [47-50]. The principal motivation for doing this was the desire to understand the hypothetical subassembly accident. The main advantages of SCARABEE are: (1) an increased diameter of the experimental loop allowing to test pin bundles (up to 37 pins instead of 1 in CABRI), and (2) a higher power in the driver core, combined with neutronic filters and variable enrichments in the bundle, makes possible a high and relatively uniform linear power in the bundle.

However, no fuel displacement measuring device (such as the hodoscope for CABRI) is available and no rapid power transients can be achieved. This reactor is thus well suited to study the melt down of small bundles in slow transients. Nevertheless, the size of the experimental devices is still small compared to the scale at which the phenomena evolve in a reactor. This has several consequences: (i) no integral test is possible to study the complete accident scenario of the subassembly blockage accident; therefore, three test groups have been defined to study separately the different phenomena. The first was concentrated on events inside the blocked subassembly (BE + series), the second on the propagation into the interassembly gaps (PI) and the third on the propagation into the neighboring subassembly (PV), (ii) the extrapolation to reactor conditions has to be made with caution and for many phenomena the physical understanding from the tests has to be modelled in codes, which may then be used to help to evaluate the behavior in the reactor, and (iii) this necessity of modelling and extrapolation required other more fundamental or analytical tests, like the molten and boiling pool series (BF) and a complementary out-of-pile test programme.

In order to enlarge our understanding of the physical phenomena and give to the modelling a more general value, other accident scenarios have also been considered, such as a slow pump coast down (APL tests) and a total blockage at the entrance of a subassembly before power rise (BE).

The 14 tests performed up to 1990 since 1983, are presented in the matrix (Figure 1.1.3.2). All these tests have been done with fresh fuel. A BE + test with an irradiated 37 pin bundle is planned and more are under consideration for a possible future programme. In preparation to this test, a single pin irradiated total instantaneous blockage test has been run in CABRI (BTI CABRI). The evolution of the experimental scenario in the BE + test series is given on Figure 1.1.3.3. First, it is note-worthy that in so many tests which resulted in melt-down and even boiling of core materials in a sodium environment, there has never been noticed any strong mechanical energy release. The evolution of the accident seems to be dominated essentially by thermal phenomena: the establishment of the pools is rather progressive and the average heat fluxes grow continuously, in a manner that is accessible to calculations. Nevertheless, local heat fluxes and material motions, and unsymmetries are almost impossible to calculate in a mechanistic way, but may play a role in the accident evolution. With fresh fuel, the potential for axial ejection of fuel out of the blocked subassembly is low;
## SCARABEE
 Melting of core materials and propagation

<table>
<thead>
<tr>
<th>TOTAL INSTANTANEOUS BLOCKAGE</th>
<th>MOLTEN AND BOILING POOLS</th>
<th>SLOW PUMP COAST DOWN</th>
<th>BLOCKAGE AT START</th>
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</thead>
<tbody>
<tr>
<td>INSIDE BLOCKED SA PROPAGATION</td>
<td></td>
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</tr>
<tr>
<td>INTER. SA. PROPAGATION</td>
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<tr>
<td>PROPAGATION TO NEIGHB. SA</td>
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<td></td>
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<td></td>
<td></td>
</tr>
<tr>
<td>MOLTEN AND BOILING POOLS</td>
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<tr>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>SLOW PUMP COAST DOWN</td>
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</tr>
<tr>
<td>BLOCKAGE AT START</td>
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<table>
<thead>
<tr>
<th>BE+</th>
<th>P1</th>
<th>PⅤ</th>
<th>BF</th>
<th>APL</th>
<th>BE</th>
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</thead>
<tbody>
<tr>
<td>83</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td>APL1</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td>APL2</td>
</tr>
<tr>
<td>84</td>
<td>BE+1</td>
<td></td>
<td></td>
<td></td>
<td>APL3</td>
</tr>
<tr>
<td>85</td>
<td>BE+2</td>
<td></td>
<td>BF1</td>
<td></td>
<td>BE</td>
</tr>
<tr>
<td>86</td>
<td>BE+3</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>87</td>
<td>BE+3 bis</td>
<td>BF2</td>
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<td>88</td>
<td>P1A</td>
<td></td>
<td>BF3</td>
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<td></td>
</tr>
<tr>
<td>89</td>
<td></td>
<td>PⅤA</td>
<td></td>
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</tbody>
</table>

**Fig. 1.1.3.2.** SCARABEE test matrix

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**Fig. 1.1.3.3.** Schematic experimental scenario of the BE+ test series, after total blockage at Os

- Sodium boiling: 2.5 s
- Dry-out: 4 s
- Clad melting: 5-6 s
- Fuel melting: 8-11 s
- Steel boiling: 14-16 s
so is the potential for ejecting fuel through the inter-assembly gaps after hexcan melt-through. This means that a large power concentration is kept at the initial failure site, and that the accident is not self-limiting by fuel dispersal before propagation into the neighboring subassemblies. This large concentration, combined with the high heat transfers in a boiling mixed $\text{UO}_2$-SS pool, also signify that propagation will run fast and that, even if detection through neutron emitters were effective after penetration of the fuel into the inter-assembly gaps, as can be deduced from the PI-A results, it would be too late to prevent the propagation of the melt into the neighbors. However, this high transfer means that the pressure will be small in the boiling pool, and thus it can be expected that the penetration of the melt into the neighbour will not be immediately total. An important sodium flow might be left for some time, giving a possibility for thermal detection at the outlet of the neighboring subassemblies and slowing down the thermal propagation. In any case, a delayed neutron signal can be assumed during the scenario, either as soon as there is penetration into the inter-assembly or at latest after propagation into the neighbour. This signal will trigger the scram after the response time which in a reactor is typically between 10 and 30 s. The open question now is to know how far the fuel propagates from the neighboring subassembly is in this time and whether the resulting debris can be successfully cooled at decay heat levels. With irradiated fuel, some important phenomena might be different. On the one hand the fission gases might constitute a pressure source for more dispersal and earlier detection. But on the other hand, the pressure might also accelerate the propagation. The plan is to investigate these phenomena in the irradiated fuel test (BE + I, a 37 pin test with 4.5 at% BU).

REFERENCES


[40] Herbst R.J. LMFBR off-Normal, Transient Test. [17], pp.II/3-1-II/3-6.


1.1.4. Molten fuel-coolant interaction

Molten steel- and fuel-coolant interactions become of interest if, during the course of a postulated accident fuel, cladding, or structural materials become molten and can contact sodium. The potential for explosive interactions between these materials has been of primary concern for some time. This concern is amplified by the fact that it has been known for years that violent interactions can occur when hot liquids come into contact with cold liquids. The occurrence of such violent interactions can be found in the nuclear as well as the non-nuclear industries. Some examples are listed in Table 1.1.4.1. [1].

TABLE 1.1.4.1. EXAMPLES OF VAPOR EXPLOSIONS

<table>
<thead>
<tr>
<th>Related Areas</th>
<th>Hot Fluid</th>
<th>Cold Fluid</th>
</tr>
</thead>
<tbody>
<tr>
<td><strong>Nuclear</strong></td>
<td><strong>Non-Nuclear</strong></td>
<td></td>
</tr>
<tr>
<td>SPERT-1, SL-1, BORAX-1</td>
<td>Aluminum</td>
<td>Water</td>
</tr>
<tr>
<td>Foundry Industry</td>
<td>Steel</td>
<td>Water</td>
</tr>
<tr>
<td>Aluminum Industry</td>
<td>Aluminum</td>
<td>Water</td>
</tr>
<tr>
<td>Kraft Paper Industry</td>
<td>Smelt (\text{Na}_2\text{CO}_3 &amp; \text{Na}_2\text{S})</td>
<td>Water</td>
</tr>
<tr>
<td>LNG Industry</td>
<td>Water</td>
<td>LNG</td>
</tr>
<tr>
<td>Volcanic Eruptions</td>
<td>Lava</td>
<td>Water</td>
</tr>
<tr>
<td>or Submarine Explosion</td>
<td></td>
<td></td>
</tr>
<tr>
<td>(Krakatoa, etc.)</td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

These energetic interactions have been attributed to the sudden conversion of liquid to vapor - a physical interaction, rather than a chemical reaction.

A thermal interaction between molten core materials and the sodium coolant, known as a Molten Fuel Coolant Interaction (MFCI) is of concern in the study of the safety of the LMFBR. In the case of a hypothetical reactivity accident which leads to disassembly of the core, an energetic MFCI could in principle add significantly to the yield and endanger the integrity of the primary containment. On a smaller scale, less energetic MFCIs occurring coherently in a group of subassemblies might lead to significant reactivity changes by inducing fuel movements or expulsion of sodium from the core. An understanding of the conditions under which MFCI’s may occur and their characteristics are thus of great importance to these aspects of reactor safety.

Many experiments have demonstrated small energy conversion ratios. The theoretical understanding of basic physical phenomena and controlling mechanism is still incomplete. This makes the required extrapolation to higher temperatures and larger masses in the \(\text{UO}_2/\text{Na}\) system, either from present experiments in this system or from simulation experiments difficult. Out of all the experiments performed on \(\text{UO}_2\) and sodium, only some out-of-pile tests by Armstrong with the injection of a few grams of sodium in liquid \(\text{UO}_2\) [2, 3] have produced evidence of a vapor explosion with the formation of a shock wave. Possible interpretation of these data have been discussed by Fauske [4]. Fauske suggested that, when liquid sodium is injected into molten \(\text{UO}_2\), some of the liquid sodium will be entrained and wet the molten \(\text{UO}_2\) surface (which in the ideal laboratory experiment can be considered free
of gas bubbles). Because of lack of nucleation sites in the liquid - liquid system (subsequent UO$_2$ freezing is not important if gas is absent), the sodium temperature is raised to the temperature limit corresponding to spontaneous nucleation. When this temperature is reached, vaporization is rapid enough to produce shock waves. The interpretation implies that in a real reactor system, even a small-mass vapour explosion as observed in Armstrong’s experiments, would be difficult to achieve because of the ample supply of pre-existing nucleation sites.

Later, Fauske [4] calculated that for oxide fuel, the maximum sodium temperatures would not exceed the spontaneous nucleation temperature of sodium in a widely mixed system. This conclusion derives mainly from the relatively poor thermal conductivity of oxide fuel. There could be instances where liquid sodium, completely entrapped in liquid fuel, could exceed the coolant spontaneous nucleation temperature (and large pressure spikes have been experimentally observed for such systems), but the energy content of such interactions is small. Rather convincing arguments were advanced which indicated that conditions for a large-scale vapour explosion in an oxide/sodium system could not occur. Though not universally accepted, this conclusion is gaining very wide support [5].

Over the years, several models have evolved for the interpretation of molten fuel-coolant interactions. Virtually all models require fuel fragmentation to enhance the surface area for thermal energy exchange and an initial period of film or transition boiling to aid in the intermixing process and/or fuel fragmentation. Sodium-fuel interactions have often been interpreted as thermal detonations [6, 7, 8] where spontaneous or induced triggering events (small shocks) cause the destabilization and collapse of stable vapour films around hot fuel particles. Knowles [17] distinguishes two alternative mechanisms of sodium vapour production: MFCI and Q*-events. An MFCI involves four distinct consecutive processes: coarse mixing, triggering, shock propagation and expansion. During the crucial coarse mixing stage, the molten core material forms a cellular structure which is enveloped in vapour. The low heat transfer rate associated with this vapour blanket conserves the thermal energy of the core material which remains largely molten. A shock wave created for example by a partial collapse of internal structure locally de-stabilizes the vapour film in the triggering process. The resulting local contact of molten fuel with liquid coolant enormously enhances the heat transfer rate and, with rapid local vaporization of the coolant, a shock wave is initiated. As well as intuitive reasoning, a considerable body of experimental evidence confirms that provided a coarse mixture is: (1) compact enough to harness the shock wave’s energy for progressive vapour film destabilisation and debris fragmentation, (2) largely molten to facilitate fine-enough fragmentation, (3) dispersed enough to contain some liquid coolant for vaporization, and (4) already fragmented and dispersed sufficiently for the thermal energy to be capable of inducing the ultimate fine-fragmentation over the millisecond timescale then the shock wave can become self-sustaining by a sufficiently rapid release of thermal energy from the melt. Due to the inertia of the surrounding liquid coolant, shock propagation is largely completed before expansion of the heated coolant (vapour or supercritical fluid) can effect an appreciable pressurization of the reactor rig vessel.

A Q*-event is a progressive conventional heat transfer process to the liquid coolant from core debris that might have been created by some separate mechanism. Rig experiments, that are nominally identical and carefully constructed for repeatability, evoke two quite different types of Q*-event: (1) a relatively fast process over a 10 ms interval, and (2) a much slower event over a 1000 ms interval.

Apart from the rate at which a prescribed amount of core debris contacts liquid coolant, the energetics of Q*- and MFCI-events are governed by the debris-size distribution.
A spheroid in perfect contact with a constant temperature environment transfers heat at a rate largely determined by its dominant time constant:

\[
\tau = \frac{(\text{Diameter})^2}{(4\pi^2 \times \text{Thermal Diffusivity})}.
\]

Thus finer debris-size elicit faster and more powerful events. Heat transfer rates in MFCI are generally much greater than in Q*-events because the debris fragmentation is intrinsically finer, and because the shock speed (~100 m/s in 3-component, 2-phase mixture) determines the contact rate of molten core debris and liquid coolant. Accordingly, MFCI are more powerful than Q*-events. Such interpretations of MFCIs assume that film boiling is initially generated upon molten fuel-coolant contact. This assumption has been questioned by others who prefer to assume that a transition boiling mode is initially established upon fuel-coolant contact. In this boiling mode, the generated vapour pockets automatically collapse when the driving pressure within the pockets becomes less than the surrounding system pressure [9-12]. This violent growth and collapse of transition boiling vapour pockets has been identified as a more credible mechanism for fuel-sodium fragmentation.

Schins and Gunnerson have used the following arguments in favour of this MFCI model [11]. The fuel-sodium interface temperature upon contact can be used as an indication of the initial boiling behaviour. The contact interface temperature \(T_{int}\) can be assessed from the solution of the parabolic heat conduction equation:

\[
T_{int} = \frac{T'(\lambda/\sqrt{\alpha}) + T'(\lambda/\sqrt{\alpha})' \text{erf}(\Psi)}{\text{erf}(\Psi)}
\]

(1)

\[
\frac{C_p(T - T')}{\Delta H_f/\pi} = \Psi \exp(\Psi^2) \left[ \frac{\lambda/\sqrt{\alpha}}{\lambda'/\sqrt{\alpha}} + \text{erf}(\Psi) \right]
\]

(2)

where superscript prime ('') refers to the coolant material. The error to the coolant function term results from the solidification of the fuel and is evaluated from the transcendental expression: although eq. (1) is time independent only for semi-infinite geometries, it is also approximately valid for finite fuel-coolant geometries when the contacting period is sufficiently short so that the thermal penetration distance is less than the characteristic dimension of either the fuel or coolant.

The boiling behaviour, based on the interfacial contact temperature, can be estimated from the following basic principles: (1) if the contact temperature is less than the saturation temperature of the coolant, boiling is impossible. For sodium at atmospheric pressure, \(T_{sat} = 1156\) K, (2) if the contact temperature exceeds the saturation temperature of the coolant (including the additional necessary superheat) and is less than the heater surface temperature at the departure from nucleate boiling \(T_{DNB}\) point, then nucleate boiling is expected, (3) if the contact temperature is greater than the departure from nucleate boiling temperatures and less than the minimum film boiling limiting superheat temperature, then transition boiling is expected, (4) if the contact temperature is greater than the minimum film boiling wall temperature \(T_{min}\) then film boiling will result, and (5) if the contact temperature equals or exceeds the superheat limit of the coolant, then film boiling must ensue. The limiting superheat temperature can be theoretically evaluated from kinetic nucleation theory.
(homogeneous nucleation) or from an applicable thermodynamic equation of state (maximum, metastable superheat). For sodium, at low pressure, the limiting superheat temperature is approximately 2100 K [8].

The predicted fuel-sodium boiling behaviours for copper (at 2300 K), stainless steel (at 1900 K), alumina (at 2310 K) and uranian (at 3120 K) in sodium are shown in Fig. 1.1.4.1. As illustrated in this figure, nucleate and transition boiling modes are predicted to dominate the heat exchange process at high and intermediate sodium subcoolings. Only when the sodium temperature nears the saturation temperature is film boiling predicted. The experimental test conditions are also indicated in Fig. 1.1.4.1.

The violent transition boiling regime is characterized by the growth and subsequent collapse of large sodium vapour bubbles or pockets. The collapse of such vapour pockets, particularly at high liquid subcoolings, impart hydrodynamic forces to the immersed molten fuel lumps that can result in rapid or prompt fuel fragmentation. An overview of this fragmentation mechanism is given in ref. [9]. Both metal and oxide fuels may undergo fragmentation by this mechanism.

As the smooth fuel particles continue to cool, solidification commences and thermal stresses act on the solidifying particles. Thermal stresses induced by temperature gradients in the solid lead to shrinkage cracking and to fracturing; if the local stress exceeds the ultimate tensile stress, failure is imminent. A review and discussion of this thermally induced fragmentation mechanism is found in ref. [9, 13-16]. The fractured fuel debris, characteristic of the oxide fuels tested, are most likely formed by this mechanism. By contrast, the smooth particulate characteristic of the metal fuels indicate that the thermoelastic behaviour of the metals prevented such fragmentation.

1.1.4.1. Out-of-pile-experiments

Most tests have been carried out in JRC-Ispra, CEA-Grenoble, AEE-Winfrith [7, 11, 12, 18, 19, 20, 21]. Condensed data on the experiments are presented in Table 1.1.4.2.

TABLE 1.1.4.2. CONDENSED DATA ON EXPERIMENTS

<table>
<thead>
<tr>
<th>Comment</th>
<th>BETULLA</th>
<th>CORECT II</th>
<th>RIG B</th>
<th>MPTF</th>
</tr>
</thead>
<tbody>
<tr>
<td>Apparatus</td>
<td>Fig. 1.1.4.2</td>
<td>Fig. 1.1.4.3</td>
<td>Fig. 1.1.4.4.</td>
<td>Fig. 1.1.4.5.</td>
</tr>
<tr>
<td>Sodium mass, kg</td>
<td>4 /160</td>
<td>6.5</td>
<td>50</td>
<td>1000</td>
</tr>
<tr>
<td>Sodium temp., °C</td>
<td>157/700</td>
<td>685</td>
<td>250/450</td>
<td>250/600</td>
</tr>
<tr>
<td>Fuel mass, kg</td>
<td>1 /2.3</td>
<td>4.9</td>
<td>0.5 /0.6</td>
<td>25</td>
</tr>
<tr>
<td>Fuel temp., °C</td>
<td>2850</td>
<td>2850</td>
<td>3400</td>
<td>3700</td>
</tr>
<tr>
<td>Fuel release</td>
<td>Gravity/ pouring</td>
<td>Sodium release</td>
<td>Expulsion</td>
<td>Gravity</td>
</tr>
<tr>
<td>Trigger</td>
<td>None</td>
<td>Sodium hammer?</td>
<td>End cap impact</td>
<td></td>
</tr>
</tbody>
</table>
Fig. 1.1.4.1. The predicted boiling behaviour for selected molten fuels in subcooled sodium
Fig. 1.1.4.2. JRC-BETULLA I Facility

Fig. 1.1.4.3. CEA-CORRECT II Facility
Fig. 1.1.4.4. AEE-WINFRITH RIG B Facility

Fig. 1.1.4.5. Test arrangement for MFTF-SUS01
In BETULLA rig (Fig. 1.1.4.2) the fuel mass was melted in a tungsten crucible, which was heated in a tungsten radiation furnace. Fuel release was either by dropping the crucible and turning it over (BETULLA I) or by gravity release (II). The mass of the interacting sodium was either 160 kg (I) or 4 kg (II). The sodium temperature was varied from 150 to 700°C. No trigger was used. Trains of pressure pulses, three of them shown in Fig. 1.1.4.6 (C), were measured.

The main result of these tests was in the analysis of the debris. Although the interactions were always mild, the debris was the same as from all other tests known to use, small and medium-scale. It was then assessed that the smooth debris generated in the boiling fragmentation transients is broken up slowly and dispersed by thermal stress [16]. As a matter of fact, the fuel is not in its final state of fragmentation. Because the fuel is full of cracks, further mechanical break-up is easy. This has to be taken into account when making use of this particle size distribution, e.g. in parametric models. Experimental UO$_2$/Na interaction tests, conducted within JRC BETULLA I facility, are presented in Table 1.1.4.3. [11, 22].

Fig. 1.1.4.6. Significant pressure histories

From basic theoretical considerations and from the interpretation of experimental pressure pulse behaviours, it has been determined that transition boiling is the dominant mode of thermal energy exchange upon fuel contact with subcooled sodium. Unlike molten fuel-water interactions, an initial period of stable film boiling is difficult to attain in fuel-sodium systems. Only when the sodium is near its saturation temperature (or slightly subcooled), and/or when the molten fuels
are at temperatures significantly above their melting points, would film boiling conditions be expected. Since an initial film boiling period is generally considered as a requisite condition for an energetic thermal detonation, the propensity for such an interaction is greatly reduced in the fuel-sodium system. Likewise, the transition boiling concept compliments the experimentally observed thermal, pressure and fragmentation behaviours.

Post-test examination of the fuel debris indicates two mechanisms of fragmentation dominate for the oxide fuels. The first is a prompt and rapid fragmentation of the molten fuel due to boiling induced hydrodynamic forces. This fragmentation mechanism produces the characteristic smooth and spherical frozen fuel particles. The second is a delayed thermal stress mechanism, which gives rise to the fractured particulate. For the metal fuels, where the thermal stresses do not exceed the ultimate tensile stress, only the first fragmentation contributes to fuel breakup.

Particle size distributions for UO$_2$/Na experiments are given in Fig. 1.1.4.7 [11, 22]. In all samples of UO$_2$, there are fractured, nearly-smooth and smooth particles. In all samples of the UO$_2$ particulate, inspection reveals that about 20% by weight are nearly smooth particles. With a simple vibrating inclined plane it was established for three samples that ~ 5% by weight are completely smooth. The rest of the particles are fractured. Fig. 1.1.4.7 shows particle distributions for these tests compared to a TREAT S-series in-pile experiment. From this diagram one can deduce that these out-of-pile interactions produce smaller particle sizes than the in-pile TREAT experiment. With respect to particle size distributions interesting results has been obtained in PNC out-of-pile experiments [23]. In addition to PNC data the TREAT S-series in-pile data, ANL out-of-pile data and SPERT CDC in-pile data are reproduced in this figure as well. Comparing the TREAT (sodium) and SPERT (water), it is noted that not even the difference in coolant substance appears to exert a decisive influence on the particle size distribution. Fig. 1.1.4.8 shows these distributions. Since the density function of the finest particle-size distribution is of importance to MFCI safety analysis, this function was obtained from the experimental data for the minimum particle diameter, by least-square fitting in the range 20 ~ 90% mass of particles smaller than indicated diameter. The result is shown by the solid straight line in this figure. This line passes the 50 and 84.13% levels at 185 and 900 μm respectively. Assuming log-normal distribution, the function becomes:

\[
 f(\log D) = 58.1 \exp(-((\log D - 2.27)^2 / 0.944)),
\]

where D represents the diameter of UO$_2$ particle in microns.

In the CORRECT II facility (Fig. 1.1.4.3) centered discs of UO$_2$ were stacked in a special crucible and heated by an induction coil [12, 21]. When gravity release caused 4.9 kg of melt to fall down in a catchpot, the valve to the furnace was closed, and the valve to a big vacuum

---

**TABLE 1.1.4.3. TEST CONDITIONS (BETULLA I)**

<table>
<thead>
<tr>
<th>Run N</th>
<th>T Na (°C)</th>
<th>UO$_2$ weight (g)</th>
<th>Collected debris (g)</th>
</tr>
</thead>
<tbody>
<tr>
<td>28</td>
<td>350</td>
<td>2000</td>
<td>1542</td>
</tr>
<tr>
<td>29</td>
<td>500</td>
<td>2000</td>
<td>1562</td>
</tr>
<tr>
<td>30</td>
<td>500</td>
<td>3000</td>
<td>-</td>
</tr>
<tr>
<td>34</td>
<td>350</td>
<td>3000</td>
<td>1842</td>
</tr>
<tr>
<td>37</td>
<td>500</td>
<td>3000</td>
<td>1800</td>
</tr>
<tr>
<td>39</td>
<td>700</td>
<td>3000</td>
<td>898</td>
</tr>
<tr>
<td>40</td>
<td>630</td>
<td>3000</td>
<td>852</td>
</tr>
<tr>
<td>41</td>
<td>700</td>
<td>4000</td>
<td>2368</td>
</tr>
</tbody>
</table>
Fig. 1.1.4.7. Particle size distribution for different experiments
reservoir opened. Thus, the starting pressure could be set at 1.2 kPa. Then, the vacuum valve was closed and the sodium valve opened. The sodium rushed in, but whether it favored mixing, either by impact or by compression, cannot be established. The participating sodium mass was 6.5 kg and was at 685°C, saturation pressure 11.6 kPa. An important pressurization was recorded, see Fig. 1.1.4.6 (A).

More than twenty experiments have been performed in this apparatus, varying such quantities as UO$_2$/Na contact mode (by changing the geometry of the interaction chamber), sodium temperature and molten fuel mass. Apart from some remarkable technological achievements, the main result of the test is the violence of the interaction; maximum pressure: 6.9 MPa, duration of the pulse: 40 ms, velocity of the expelled sodium: 7.6 m/s, measured mechanical energy: 18 KJ and conversion factor: 0.15%.

Schins [12] considers that in spite of a vigorous pressure pulse measured there is evidence that it was not a thermal detonation, and that phenomena, different from those already analyzed, such as superheating or peeling, should be considered.

One may also assume that the system remains in transition boiling, but that because of the strong confinement the system is pressurized up to 7 MPa system pressure. The bubble growth and collapse mechanism then remains effective although temperature and pressure increase. Or rather: sustained transition boiling in a constant volume will provoke a temperature and pressure transient.

It is obvious that this mechanism cannot work in an almost unconfined system such as BETULLA. In the geometry of BETULLA, pressurization is very difficult because of the big sodium and cover gas volumes. This lack of confinement promotes heat loss, even through vapour, at constant system pressure and prevents pressure build-up.
The interpretation of two experiments has been conducted [21]. The extent of fragmentation in both interpretations was similar: fragmentation of 35% of the fuel mass into fine particles was sufficient to explain experiments N 18 while for experiment N 22 this percentage was slightly greater at 39%. These percentages effectively represent the amount of fuel participating in the interaction, as the thermal time constants of the larger fuel particles are too long for the almost-instantaneous energy transfer of a violent thermal interaction. The experimental interpretations by use of the solidification - induced fragmentation model verify that:

1) the kinetics of solidification are rapid enough to explain an explosive thermal interaction, and
2) this fragmentation scenario permits energy transfers of sufficient magnitude to provoke a thermal interaction.

In the rig B (Fig. 1.1.4.4.) masses of sodium of about 50 kg were heated to temperatures between 250 and 450°C [7, 12]. System pressure was varied between 0.1 and 0.6 MPa. The fuel mass of about 0.5 kg was generated by a thermit reaction, and had a very high temperature of about 3400°C. The end cap of the charge container was ejected downwards by a spring mechanism and this firing of the end cap allowed for the release, under pressure, of the fuel melt. The impact of the cap is strong enough to act as a trigger in water, but represents a weak trigger in sodium, see Fig. 1.1.4.6 (B1). Pressure records, as shown in Fig. 1.1.4.6 (B2), indicate well developed peaks in the roof, with rise times of ~ 5 ms, explained as initial expansion immediately following release. Often, other cover gas compression peaks are found. No correlation of a rather startling number of fuel drop explosion peaks, as in Fig. 1.1.4.6 (B1), measured in the pool, with the compression peaks, is possible. The most important results of these tests are the characteristic pulses measured in the sodium pool. These sharp positive pulses indicate unambiguously the occurrence of film boiling. In other experiments performed in Rig B, a non-releasing container holding 5 kg of thermit was used and the sodium, in stainless steel or nickel capsules, was placed in the thermit powder [7]. Firing the charge in these experiments resulted in the melting of the capsules and contact between sodium and UO₂. Six such firing were carried out for sodium quantities of 5 g, 10 g and 20 g using single and multiple capsule arrangements. Thermal interactions between UO₂ and sodium investigated in pool and capsule geometries allowed to conclude as follows. In the pool experiments, weak triggered and spontaneous interactions were observed with efficiencies based on the fraction of debris fragmented ranging from 0.3% to 0.7% of Hicks-Menzies. These figures are an order of magnitude less than those found for UO₂/water interactions in a similar geometry. However, they should be treated with some caution as they depend on UO₂ debris size data and a subsidiary experiment showed that this may not have been characteristic of a thermal interaction. In the sodium capsule experiments it was concluded that fragmentation was needed to give sufficient heat transfer rates to explain the most rapid pressure rises (sodium vaporization rates) obtained. Liquid-liquid contacts appeared to be likely also.

The Molten Fuel Test Facility (MFTF) [20] was commissioned to study thermal interactions between molten fuel and reactor coolant, either water or sodium. The large (1.77 m³) reaction vessel was designed for operation at a working pressure of up to 3.3 MPa at a maximum temperature of 600°C and up to 24 kg of molten UO₂ can be introduced into around 1.3 Te of coolant. The first experimental series, the Scale-Uranian-Sodium (SUS) experiments have been completed [20]. These experiments investigated the effect of scale on interactions involving sodium by replicating earlier experiments on a larger scale. Two modes of release were employed in the SUS experiments. The first three experiments were carried out in the free release mode with the melt being released into the surrounding sodium with no restrictions. In the remaining three experiments the expansion of the molten charge products into the surrounding sodium was physically restricted by means of a stainless steel tube supported on the base of the reaction vessel and surrounding the charge container. In experiments with 24 kg of molten fuel, multiple (up to 7) small interactions were observed with yields between 8 and 137 KJ. The number of interactions increased with both scale and system pressure. This was attributed to the more efficient mixing that occurred during the associated increase in melt transit time. In previous
water experiments between 1 and 3 energetic (up to 850 kJ) interactions were observed. The average interaction efficiency in these large-scale sodium experiments was estimated to be 4.3% of the total available thermal energy of melt mass below 250 μm. This was an order of magnitude higher than found at small-scale. However, this result was dependent upon the debris particle size distributions being unaffected by processes other than MFCI. This is probably not the case particularly for the small-scale tests. The estimated efficiencies for the large-scale sodium and water experiments were quantitatively similar. This is consistent with the view that condensation effects are important in determining the yield of an interaction. Some small-scale tests in UO2/Na system using the dropping mode of contact have been performed in India [24]. The maximum pressure peak registered in these runs was 440 KPa. The molten mass of UO2 underwent extensive fragmentation.

1.1.4.2. In-pile-experiments

Mechanical energy release during MFCI in-pile conditions has been estimated in TREAT, CABRI and SCARABEE experiments. The main features of CABRI-experiments when thermal interactions have been observed are given in Table 1.1.4.4. [25].

<table>
<thead>
<tr>
<th>Experiment</th>
<th>A3</th>
<th>A4R</th>
<th>A4</th>
<th>AI2</th>
<th>AI3</th>
<th>AH3</th>
</tr>
</thead>
<tbody>
<tr>
<td>test fuel</td>
<td>UO2</td>
<td>UO2</td>
<td>UO2</td>
<td>(U,Pu)O2</td>
<td>(U,Pu)O2</td>
<td>(U,Pu)O2</td>
</tr>
<tr>
<td>burn-up (%)</td>
<td>0</td>
<td>0</td>
<td>0</td>
<td>1.87</td>
<td>1.29</td>
<td>4.8</td>
</tr>
<tr>
<td>TER (KJ/g)</td>
<td>1.44</td>
<td>1.79</td>
<td>1.87</td>
<td>0.88</td>
<td>1.29</td>
<td>1.34</td>
</tr>
<tr>
<td>P/Po</td>
<td>624</td>
<td>716</td>
<td>761</td>
<td>109</td>
<td>250</td>
<td>398</td>
</tr>
<tr>
<td>TF (ms)</td>
<td>58.3</td>
<td>56.7</td>
<td>55.5</td>
<td>230</td>
<td>82.5</td>
<td>78</td>
</tr>
</tbody>
</table>

TER: transient energy release during the TOP,
P/Po: ratio of peak power to nominal power just before TOP-trigger;
TF: time of test pin failure after TOP-trigger.

For the characterization of MFCI phenomena only those sequences are of importance when direct contact between ejected liquid fuel and liquid sodium presumably lead to large rates of mechanical energy release. Estimates from HODOSCOPE measurements indicate that in experiment A4 (AH3) a mass of 13 g (8 g) fuel was ejected into the coolant channel during the first 3 ms after test pin failure. A rough estimation of the fuel enthalpy accumulated in this mass up to 6 ms after failure amounts to 28 KJ (14 KJ). Subtracting the enthalpy necessary to reach the liquid state (1 KJ/g) results in an amount of heat energy of 15 KJ (6 KJ) which is available for the conversion of thermal into mechanical energy. In fact, only 0.5% (2.3%) of heat has been converted. The corresponding efficiencies of the other experiments are indicated in Table 1.1.4.5.

In many of the SCARABEE tests several occurrences of sodium re-entry have been seen during the course of the test, following reactor trip and following the attempt to re-establish the flow at the end of the test. In each case the energy yield and the peak pressure were small but, nevertheless, sufficient to disperse molten materials and effect blockage formations [26]. Different small pressure events due to sudden sodium or steel vaporizations have been observed, which may play a role in material relocation; for example, the upward ejection of steel and blockage creation before boiling pool expansion (BE + 1), and projection of molten materials on to the hexcan walls with the potential to lead to their melt-through (BE + 3 bis). None of these pressure...
events developed a significant mechanical energy neither could they be designated as an energetic fuel-coolant interactions [27].

<table>
<thead>
<tr>
<th>Experiment</th>
<th>A3</th>
<th>A4R</th>
<th>A4</th>
<th>AI2</th>
<th>AI3</th>
<th>AH3</th>
</tr>
</thead>
<tbody>
<tr>
<td>Short term Yield (J)</td>
<td>30</td>
<td>84</td>
<td>66</td>
<td>50</td>
<td>114</td>
<td>133</td>
</tr>
<tr>
<td>Long term Yield (J)</td>
<td>56</td>
<td>125</td>
<td>120</td>
<td>85</td>
<td>188</td>
<td>256</td>
</tr>
<tr>
<td>Conversion Efficiency (%)</td>
<td>0.3</td>
<td>1.5</td>
<td>0.5</td>
<td>1.1</td>
<td>1.3</td>
<td>2.3</td>
</tr>
</tbody>
</table>

The TREAT autoclave tests on failure-related phenomena associated with potential fuel-coolant interactions yielded thermal-to kinetic energy efficiencies in the range (0.01%-0.02%) [28-32]. One of the questions concerning the general problem of fuel-coolant interactions is the discrepancy between the conversion efficiencies obtained for UO$_2$-water and UO$_2$-sodium experiments in TREAT. The water experiments result in an order of magnitude higher conversion ratio than do the sodium experiments. As was discussed in the context of the H-2 experiment [32], film condensation appreciably reduces the net amount of fuel energy released to the expanding vapour. This results in a reduction of conversion efficiency. However, in the case of water experiments the effects of condensation at surfaces covered by liquid film would be less.

CONCLUSIONS

Numerous out-of-pile and in-pile experimental studies of molten fuel-sodium interaction have not revealed any phenomenon that could be classified as a severe vapour explosion which are often observed in molten fuel-water systems. The main reasons are: 1) a high temperature of spontaneous sodium nucleation, 2) a large extent of sodium subcooling up to the boiling temperature and 3) the good thermohydraulic properties of sodium as coolant. In the opinion of many authors, fuel fragmentation occurs under transient boiling conditions of sodium, but not under film boiling as with water. The secondary mechanism of fragmentation are thermal stresses on the surface of cooling particles. This mechanism, however, acts with delay, and its contribution into the total interaction effect is negligible.

A vigorous vapour explosion can be observed only at sodium injection into the bulk of molten fuel where the spontaneous nucleation temperature can be reached.

Non-coherence of interaction of a large amount of molten fuel with sodium leads to the division of the process into a number of small interactions separated in time.

The risk of destruction of a fuel subassembly or reactor as a whole seems highly improbable due to the impossibility of formation during a short time of compact masses of molten fuel and its coherent interaction with sodium [17].

Further investigation of molten fuel-sodium interaction is needed for better understanding of both fine physical effects and phenomenological processes as applied to reactor plants from the viewpoint of mechanical energy release.
REFERENCES


1.1.5. Post-accident heat removal

In the event of a severe accident in a sodium cooled fast reactor, the molten core materials may interact with liquid sodium and thus result in quenching and freezing fragmentation. This fragmented debris may settle on horizontal surfaces within the reactor vessel to form debris beds. The fuel in these debris beds will be heated by radioactive decay of retained fission products and actinides and can represent a hazard to the reactor vessel integrity unless adequate heat removal is assured. The under-sodium decay heat generating bed can have three destined regions with predominantly different heat transfer mechanisms. These are subcooled, boiling and dryout zones. The heat from the volume heated bed on reaching certain temperature conditions is removed to surrounding sodium only by heat conduction. Then, at some critical Rayleigh number, these arises natural convection that intensifies the heat transfer process. However, if decay power exceed the heat removal capabilities due to single-phase sodium convection then its temperature reaches the boiling point and subsequent process is going on more actively with heat absorption by coolant vaporization followed by its condensation within a cold volume above the bed. If the decay power level is large enough to evaporate all the liquid flowing into the bed before it reaches all parts of the debris, local dryout of the debris will occur and a dryout zone appears where heat removal can take place only by means of porous bed conduction, radiation, and convection by vapour. The dry porous bed conductivity being low (especially for the oxide fuel), its temperature can reach the melting point of steel and then of fuel. From the above it follows that depending on the conditions both steady state heat removal and its disturbance due to dryout are possible. In order to carry out validation calculations on reactor safety in the event of core melting accidents, experimental studies of the behaviour of such beds under various conditions are required.

Experimental research should deal with the investigation of: the bed formation process (size distribution of particles within the bed, bed thickness, bed porosity, etc.); the dependence of incipient dryout power on various parameters and; molten bed formation after dryout. Most experiments conducted up to now have been aimed at the investigation of dryout power. The dryout power value is to a large extent affected by the following parameters: particle characteristics (size, shape, height distribution, etc.); the bed depth; heat generation and its distribution; cooling conditions of the bed bottom (of the plates, catcher, etc.); subcooling at the bed top or bottom; coolant property; geometrical characteristics, etc. A very important parameter determining the rate of heat removal from the bed is its permeability for the convection of the coolant from bottom upwards (natural or forced). This process is feasible for a bed resting on the perforated surface.

For the investigation of mechanisms of heat removal from the heat generation bed out-of-pile and in-pile experiments have been conducted. The bed behaviour is determined by three characteristic sets of parameters: (1) materials: type, amount, spatial and time distribution, (2) Heat generation: absolute value, distribution between fuel and steel, as well as over the radius and with height, and (3) Boundary conditions: rigid or melting walls, the presence of frozen crust and external cooling. These three sets of parameters can be realized only under in-pile conditions. As to rig experiments, the volume heat generation can be created only with the use of electromagnetic phenomena: direct heating by electrical current, induction or microwave heating. There are some basic limitations on meeting all the above sets of parameters for each of heating procedures. E.g., heat will be mainly generated within metallic materials (sodium, steel) but not within ceramic fuel. Detailed requirements on experiments conduction, the consideration of some features of the investigation of various aspects of the problem, as well as some proposals on creation of a special-purpose reactor have been presented in [1]. There are about 60 publications in literature on experimental research of the heat-generating debris bed behaviour. Here we shall not list all of them but specify only those where practically all information has been summarized [2-36].
1.1.5.1. Out-of-pile researches

Sowa et al. [2] performed some of the earliest research in the field. They investigated sodium-filled UO\(_2\) beds with bottom heating but did not achieve dryout. They also noted a large porosity in beds of fine particles (0.025-0.100 mm). Gabor et al. [3] used water-UO\(_2\) beds with heat generation in the water (direct electrical heating). They were the first to note channels at the top of the bed and observed a decrease in dryout flux as the bed thickness increased. They also noted that the dryout flux for bottom-heated beds was less than in volume-heated beds. In [4] they studied sodium-UO\(_2\) beds with electrically heated sodium to simulate volume-heating of decay heat. Keowin [5] studied inductively-heated steel beds and lead beds in water. Dhir and Catton [6, 8] used inductive heating with steel beds and lead beds in water, acetone, and methanol. They noted that the dryout heat flux was independent of bed thickness in deep beds (greater than about 50 mm, depending on the fluid). Sowa et al. [7] in experiments with steel beds in water noted similar dryout fluxes with heating in the water or in the particles. Dhir [10] investigated dryout fluxes in beds with particle size distributions. Le Rigoleur [9] proposed using a formula to determine the effective particle diameter for a distribution of particles sizes:

\[
d = f_s (\Sigma w_i/d_i)^{1/t}
\]

where \(w_i\) is the weight fraction of particles with sieve diameter \(d_i\), \(f_s\) is the shape factor equal to 1 for a sphere and to 0.78 for rough particles. Dhir confirmed the applicability of this formula if particles less than 0.1 mm are not taken into account. It agrees with that has been noted by Gabor et al. in [4]. Dhir et al. [11] investigated heat removal from steel particles in acetone with both a subcooled overlying pool and with a subcooled base. They noted only a minor influence of subcooling the pool. They interpreted the downward heat removal to be conduction only. Squarer et al. [13] studied dryout flux in inductively-heated beds with forced convection of coolant through a porous plate on which the beds were supported. In this case the dryout flux increased considerably. They also studied the dryout fluxes in beds of different diameters. They initially reported that the dryout flux decreased with increasing bed diameter, however, later studies showed that this was due to an error and there was no dependence of dryout flux on bed diameter. Barleon et al. [14] were the first to study dryout fluxes for beds with particles larger than one millimeter in diameter (with inductive heating). The dryout fluxes they obtained were much smaller than predicted by most models. This was the first data to indicate that the dryout flux does not depend strongly on particle diameter with large particles. Trenberth et al. [15] heated the beds by passing an alternating current through the touching particles. They observed decreasing dryout flux with bed thickness (as first noted by Gabor et al. [3] and related it to the influence of capillary force that was in accordance with the model by Shires et al. [12]). Gabor et al. [16,18] studied bottom-and volume (inductively)-heated beds involving copper and steel particles with water, acetone, methanol, isopropanol and freon-113. They noted that the dryout fluxes from a volume-heated bed was generally two or three times that from a similar bottom-heated bed. Dhir et al. [17] studied the dryout fluxes in the "very deep" acetone-steel beds and noted no change in the dryout flux in beds from 50 mm to 400 mm thick. Jones et al. [19] and Gabor et al. [20] continued the study by Gabor et al. [16] with water, acetone, methanol, isopropanol and freon 113 for deeper beds and with different bed diameters. They also did not note any dependence of the dryout flux on the bed diameter, which agreed with the conclusions presented in [13]. They noted that the dryout flux from a volume-heated bed is two times that from a bottom-heated bed. Barleon et al. [21, 25] extended their earlier work [14] on beds with large particles and noted that the dryout flux varied with the square root of particle diameter. Naik et al. [23] reported measurements of the pressure drop across beds with boiling at forced circulation. They expressed their results in terms of the quality of the exit vapour. Somerton et al. [24] reported that thickness of the overlying coolant pool above the bed can have an effect upon the dryout flux. Summarized data on most out-of-pile experiments carried out up to 1982 are presented in [39].
Barleon et al. [27] studied a steel and bronze bed in water and freon-113. The particle size varied from 0.04 to 16 mm including stratification in size. The boundary conditions included saturated and subcooled fluid, as well as adiabatic and cooled bottom and lateral sides of the experimental section. In the paper a large number of experimental data obtained at Karlsruhe are summarized which were used for the analysis of accidental situations in SNR-300. The main conclusions of this work are as follows:

1. Both the dependence of the dryout flux on particle size and on the bed height agrees well with the theoretical predictions. Absolute values of calculated dryout fluxes are generally a little high (10 to 20%).
2. Subcooling of the overlying coolant pool can prevent channel penetration, but for the beds studied here the channeling had a small influence on the dryout fluxes.
3. Cooling of the bed bottom increases the dryout heat flux. In this case capillary force has a strong effect.
4. Stratification (decreasing a particle size monotonically with elevation) causes a strong reduction of the dryout flux (compared to the mixed bed) if the bottom is adiabatic. In case of cooled bottom the dryout fluxes for a stratified bed and a mixed bed are comparable (at least for the beds characteristic of the fast reactor).
5. The experimental results are generally well described by theory (the dependence on particle size, bed height, cooling of the bottom and stratification). Remaining discrepancies that should be resolved are: a) overprediction of absolute dryout heat fluxes, b) severe underestimation of the vapour fraction, c) shortcomings in the channel length calculation, which cause severe discrepancies for stratified beds.

In the journal issue where this work was published some more articles on this subject were reported and presented at the Meeting on 'Post-Accident Debris cooling', held at Karlsruhe in July, 1982. Some results of these experiments were briefly presented also in [28].

1.1.5.2. In-pile experiments

As was already explained above, in rig experiments it is difficult to ensure all conditions required to simulate heat removal from the debris bed. E.g., with simulating decay heat release in fuel by means of heating the fluid in which the bed is, it is impossible to study its behaviour at its partial dryout. Therefore, all rig experiments were terminated at dryout.

An extensive program to study the coolability of debris beds which may be formed during reactor accidents was performed under in-pile conditions at Sandia National Laboratories from 1977 up to 1985. This programme was performed under sponsorship of US/Nuclear Regulatory Commission, Joint Research Center of the European Communities and the Power Reactor and Nuclear Fuel Development Corporation of Japan. The results of the studies were reported in [29-34]. The program of investigations included 12 experiments which covered the whole range of parameters to be studied: the bed height, sodium subcooling; particle size and stratification; bottom heating; and the behaviour of molten debris. The main parameters of the experiments and their results are presented in Table 1.1.5.1. [34].

First experiments (D1-D9) were conducted under conditions of heat insulation of the experimental capsule’s bottom and sides to simulate a one-dimensional arrangement with simple boundary conditions. In the experiments D10 and D13 the bed bottom was cooled, simulating structures or a catcher inside the reactor on which molten core particles are settled after an accident. The experiments DC1 and DC2 with a dry capsule were carried out to investigate heat removal in the bed at high temperatures including melt of fuel and steel thus simulating a dryout zone in the particle bed. The bed height varied from small (load less than 300 kg/m²) in the first experiments to a range of 300 kg/m² to 900 kg/m² (58 mm to 160 mm). Sodium subcooling
<table>
<thead>
<tr>
<th>Experiment (date)</th>
<th>Bed dimensions, mm</th>
<th>UO₂ mass, g</th>
<th>Particle size, μm</th>
<th>Porosity, %</th>
<th>Stratification (layers)</th>
<th>Bottom cooling, kW/m²</th>
<th>Subcooling, °C</th>
<th>Dryout powers, W/g</th>
<th>MPT, °C</th>
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<tr>
<td></td>
<td>Height</td>
<td>Diameter</td>
<td>Range</td>
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<td></td>
<td></td>
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<td>D1 (1977)</td>
<td>58</td>
<td>102</td>
<td>2430</td>
<td>100 to 1000</td>
<td>350</td>
<td>48</td>
<td>no</td>
<td>no</td>
<td>390 to 500</td>
</tr>
<tr>
<td>D2 (1977)</td>
<td>106</td>
<td>102</td>
<td>4870</td>
<td>100 to 1000</td>
<td>270</td>
<td>43</td>
<td>no</td>
<td>no</td>
<td>310 to 460</td>
</tr>
<tr>
<td>D3 (1977)</td>
<td>158</td>
<td>102</td>
<td>7290</td>
<td>100 to 1000</td>
<td>290</td>
<td>43</td>
<td>no</td>
<td>no</td>
<td>310 to 480</td>
</tr>
<tr>
<td>D4 (1979)</td>
<td>82</td>
<td>102</td>
<td>3650</td>
<td>100 to 1000</td>
<td>240</td>
<td>44</td>
<td>no</td>
<td>no</td>
<td>195 to 460</td>
</tr>
<tr>
<td>D5 (1982)</td>
<td>104</td>
<td>102</td>
<td>5820</td>
<td>40 to 4000</td>
<td>173</td>
<td>31</td>
<td>no</td>
<td>no</td>
<td>200 to 364</td>
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<tr>
<td>D6 (1981)</td>
<td>114</td>
<td>102</td>
<td>4870</td>
<td>100 to 1000</td>
<td>47</td>
<td>8</td>
<td>no</td>
<td>no</td>
<td>315 to 485</td>
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<tr>
<td>D7 (1982)</td>
<td>74</td>
<td>102</td>
<td>3500</td>
<td>100 to 1000</td>
<td>41</td>
<td>11</td>
<td>no</td>
<td>130 to 390</td>
<td>0.29 to 0.25</td>
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<tr>
<td>D8 (DC1) (1983)</td>
<td>70</td>
<td>80</td>
<td>2138</td>
<td>100 to 1000</td>
<td>240</td>
<td>41</td>
<td>no</td>
<td>275</td>
<td>Dry</td>
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<tr>
<td>D9 (1982)</td>
<td>77</td>
<td>102</td>
<td>3500</td>
<td>40 to 4000</td>
<td>14</td>
<td>43.5</td>
<td>no</td>
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<td>D10 (1984)</td>
<td>160</td>
<td>102</td>
<td>8140</td>
<td>40 to 4000</td>
<td>173</td>
<td>38</td>
<td>no</td>
<td>450</td>
<td>240 to 510</td>
</tr>
<tr>
<td>D12 (DC2) (1984)</td>
<td>70</td>
<td>80</td>
<td>1530</td>
<td>100 to 1000</td>
<td>270</td>
<td>39</td>
<td>no</td>
<td>180</td>
<td>Dry</td>
</tr>
<tr>
<td>D13 (1985)</td>
<td>160</td>
<td>102</td>
<td>7350</td>
<td>40 to 4000</td>
<td>43.5</td>
<td>24</td>
<td>550</td>
<td>300 to 510</td>
<td>0.9 to 3.0</td>
</tr>
</tbody>
</table>

*Measured peak temperature.
varied from 60°C to 500°C. Particles for the experiments were obtained at the Los Alamos National Laboratory by crushing ceramic UO$_2$, firing in hydrogen to ensure the required stoichiometry, and sieving into particle size ranges. In the experiments D1-D4, D6, D7, DC1 and DC2 the particle size was 0.1 to 1 mm. The second group of particles involved a more representative set of particle size corresponding to those obtained at molten fuel-sodium interaction, 0.04 to 4 mm. In the experiments D1-D5, D10, DC1 and DC2, the particles were homogenized in size. In D6, D7, D9 and D13 the particles were stratified in size to simulate a situation arising with particles settling within sodium volume with finer particles being in the upper part of the bed. Porosity and permeability of the bed also play an important part in heat removal from the bed. Porosity in D-series experiments varied from 31% to 48% depending on particle size, stratification and bed filling procedure.

The first three experiments D1-D3 were mainly directed at the demonstration of the technique of experiments conduction in the bed with heat generation as a result of nuclear fission. These experiments were carried out in the ACPR reactor (Annular Core Pulse Reactor) where a maximum power deposition of 1.28 W/g in UO$_2$ particles could be obtained.

Later experiments were carried out in the ACRR (the Annular Core Research Reactor) where bed power up to 4.5 W/g was provided (for comparison, decay heat release 10 minutes after reactor shutdown is ~ 2 W/g). The experiment capsule was located in the central reactor cavity. This capsule included thermocouple, ultrasonic thermometer, and pressure transducer instrumentation and provided cooling at the top and bottom of the debris bed. The fuel particles were contained within a high temperature crucible with double containment.

**The effects of subcooling and channeling.** Experiment D2, the first in-pile experiment to achieve two-phase heat removal anddryout, was designed to study the effect of sodium subcooling (310 to 460°C) on dryout. Space in the experiment D3 a deep bed (158mm) with subcooling as in D2 was studied. In D4 a shallower bed (28mm) was studied over a wide range of subcooling (195°C to 460°C). In these experiments it was shown that in the single-phase coolant region good heat removal from the bed due to conduction and natural convection was realized. For calculating heat removal under the single phase convection the following experimental expression can be used:

$$Nu = (Ra / 0.776)^{0.34}$$

After reaching dryout, a dry zone in the bed appeared which was stable, and no fast increase of fuel temperature and no fuel melting occurred.

An important result was obtained from experiments D2, D4, D6 and D7 on the influence of sodium subcooling above the bed by the dryout flux. In the experiments D2 and D4 a considerable increase of the dryout flux (a factor of 1.7 and 4.5 times, respectively) following a change in sodium subcooling. This increase was not observed in the experiment D3. At a temperature of the overlying sodium close to saturation the main mechanism of heat removal is connected with channeling in the bed by an upward vapour flow. As the subcooling is decreased, vapour channels form and thus provide a highly effective path for energy removal and increase the dryout threshold significantly. An increase of sodium subcooling suppresses channeling and so sharply reduced the rate of heat removal from the bed and respectively the dryout flux. That is why a sharp increase of the dryout flux in D2 and D4 was observed at subcooling reduction. The results of experiments are presented in Fig. 1.1.5.1. (If in the upper part of the bed no channeling takes place then with an increase of subcooling the dryout flux naturally increases). As was shown by these experiments at a subcooling above 325°C no channeling occurs. Thus the theory that high subcooling suppressed channeling was confirmed.
Fig. 1.1.5.1. Dryout power vs sodium subcooling for experiments D2, D4, D6 and D7

Fig. 1.1.5.2. Dryout power vs sodium subcooling for experiment D9
Increases in heat removal were observed also in the stratified experiment D7 and were the subject of a through investigation during the D9 experiment (Fig. 1.1.5.2). The channels were reteatable and reversible; an increase and decrease in power would alternatively form or collapse the channels with an attendant change in coolability.

**Sodium superheating.** In the experiments D2 and D4 some "disturbances" were noted which appeared and altered the characteristics of the bed; the mechanism of this phenomenon was understood only after the conduction of the experiment D4. These "disturbances" were attributed to the sodium superheating. In D2 and D4 such "disturbances" were followed by channeling. However, from these experiments it is not clear whether superheating is a necessary or a sufficient condition for channeling. Superheat occurrences were also investigated during the stratified debris experiments D6, D7 and D9. These experiments clearly demonstrated that superheat disturbances were neither sufficient nor necessary for the formation of channels. Experiment D9 indicated that, if the vapour flow that occurs as a result of superheat flashing is highly vigorous, it may be sufficient to disrupt any bed stratification and to increased bed porosity, which would lead to increased coolability.

**Particle stratification.** Particle stratification may take place under the action of gravitation when the settling of particles on surfaces occurs. Until these experiments there were contradictory opinions about the effect of the stratification on bed coolability. Firstly, an increased permeability at the bed bottom can lead to an increase of convection and as a result to cooling improvement. Secondly, decreased coolability can occur as a result of decreased permeability of the top of the bed that influences the total heat flux leaving the bed. Thirdly, decreased coolability can also take place because of the capillary pressure action which causes a decrease in liquid saturation at the bottom of the bed by drawing the liquid into smaller particle regions (i.e., upwards). The D6 experiment was the first in the program to investigate particle stratification. In this experiment a dryout power of only 0.35 W/g was observed at a sodium subcooling of 450°C as compared with 0.76 W/g for the otherwise identical but uniformly mixed D2 bed. Particle stratification appeared to suppress single-phase convection completely, and it significantly reduced the packed (unchanneled) bed dryout power. Dryout occurred immediately following the onset of boiling in a region of the bed composed of large particles. Evidently, in stratified beds the effects of capillary forces play an important role in the coolability of debris. These forces significantly reduced the power required for dryout in a stratified bed by retaining the liquid in regions of small particles and thus preventing liquid entry to regions of large particles. Further, the D9 experiment demonstrated that in the boiling region only a small degree of stratification was required to significantly influence the dryout power.

**Bottom cooling and postdryout phenomenon.** The behavior of UO$_2$ debris following dryout has been studied in experiments D10 and D13 to temperatures of 2600°C. Bottom cooling in the D10 experiment was varied over a wide range from 50 to 460 kW/m$^2$, which spanned the amount of heat removal that might be provided by passive flat-plate core catchers. With a packed bed, the dryout data were consistent with coolability models. Dryout powers of 0.42 to 0.58 W/g were not significantly different from the 0.39- to 0.42- W/g dryout powers observed in the D3 experiment, which had a similar bed height but was bottom insulated. The D3 experiment also had higher porosity than the D10 experiment (see Fig. 1.1.5.3). Dryout in the channeled D10 bed was measured to range from 1.0 to 1.5 W/g, and the UO$_2$ temperature was raised to just below the urania melting temperature with a dry zone about 6 cm thick at a power of 1.6 W/g. The dry zone appeared to be quite stable and was maintained for a period of three hours. Downward heat flow was noted in part of the boiling zone, presumably driven by capillary forces, as predicted by models [3]. Figure 1.1.5.4 shows the range of data points achieved in the D10 experiments for downward heat flux related to structural members 4 and 10 cm thick. The D13 bed, a stratified bed, had the same particle distribution as D10. After a superheat flash the bed became highly channeled and exhibited very high dryout powers (0.8 to 2.6 W/g). In contrast to D10,
the D13 experiment showed significant time-dependent behavior as the dry zone shifted around in the bed during the course of the dryout. Such phenomena cannot be described by one-dimensional steady-state models. Stable dryouts were obtained at 3 W/g and a maximum measured temperature of 2400°C.

The behavior of debris following dryout was studied in the DC experiments by simulating only the dry portion of debris beds and using argon gas to simulate sodium vapor. These experiments are an extension of the molten pool series, which investigated pure and mixed UO$_2$.
and stainless steel beds in a helium atmosphere [35-36]. In DC1, UO₂ debris was heated to a peak temperature of more than 3000°C; in DC2, a mixed bed of UO₂ and stainless steel debris was studied at temperatures of up to 2600°C. The beds were top and bottom cooled and side insulated, which simulated the conditions that would exist in the sodium-cooled experiment.

Thermal characteristics of the UO₂ bed were investigated in three steps from 1000°C to the melting point of UO₂. Preliminary analysis indicated that the measured temperature profiles are best predicted by using a composite model for the total effective conductivity consisting of Willhite's model for pure conduction in a porous media [37] along with Luikov's method for adding radiation [38].

Approximately 50% of the urania in DC1 melted. A dense, stable crust formed around the pool by molten material drawn into the debris matrix by surface tension and freezing. As melting progressed, a void formed above the pool within the bed. In DC2, the coolability of the composite bed increased substantially, subsequent to steel melting. However, examination of the bed showed only small agglomerations and little preferential migration. This behavior is markedly different from that seen in earlier furnace tests and in the five in-pile molten pool tests. Material motion was dominated by thermal-gradient-driven vapor transport.

The general conclusion which can be drawn from the D-series of experiments performed is as follows. The experiments have allowed to understand important physical phenomena that govern the coolability of debris bed and have provided essential data for modelling these phenomena.

CONCLUSIONS

The conduction of experimental investigations of the processes related to degraded core debris is the requisite condition for the validation of fast reactor safety during severe accidents.

Such investigations have gained rather wide distribution. By the present time several hundreds of experiments have been carried out, including the in-pile experiments at the Sandia National Laboratories, (the D and DC-series).

Experimental tests conducted up to now were mainly directed at studying the influence of various characteristics of the fuel particle (or fuel simulators) beds and external heat removal characteristics on the critical heat fluxes. The data obtained have considerable scatter. The greatest influence upon the critical heat power is due to the fuel particle size and their distribution over the bed height (stratification), the bed thickness, coolant subcooling, the cooling conditions of the plate (catcher) underlying the bed, etc.

The behaviour of the bed in the case of a dry zone formation is not clear enough as yet. Under these conditions as was shown by the DC-series experiments, that a stable heat removal up to the secondary melting-down of fuel is possible but the characteristics of the dry part of the bed have not been studies especially with steel being present in it. For the development of analytical models it is necessary to know heat transfer properties of the bed including the case following molten zone formation.

The available experimental data do not allow us to predict with certainty the transition from "packed" boiling to "channeled" boiling and the corresponding change in the critical heat flux.
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1.2. ANALYTICAL STUDIES

1.2.1. Methods and codes for thermohydraulic analysis of subassembly behaviour during accident conditions

Among the trends along which the development of the local characteristic analytical methods is carried out, three of them should be noted: the distributed parameters method, the porous body model and the subchannel method. In the distributed parameters method the velocity and temperature distributions are sought that satisfy Reynolds equations for momentum and energy transport. The porous body model assigns some inherent resistance and heat sources to the coolant flow in subassemblies; its complete equations being similar to Reynolds equations. The subchannel approach gives a more accurate practical representation of the fuel pin bundle than the porous body model. This method can represent phenomena whose characteristic length equals subchannel pitch.

1.2.1.1. Single-phase condition

The methods of local characteristics calculation have been developed as applied to solving the equations of motion and energy in a differential form. They include the analytical methods, the method of finite elements and the variational difference method. In a number of works the analytical methods of solving the problem of heat exchange in the subassembly region under stabilized flow conditions were being developed [1-7]. The drawbacks are complexity of calculations and approximations required taking into account of the velocity field structure and turbulent transfer characteristics. The most widely used among the local methods are the method of finite differences [8-11] and the method of finite elements [12-13]. A drawback of these methods is the limitation of the calculation region which is connected with the necessity to use detailed division into multipoint pattern, and, besides, these methods, as a rule, approximately take into account the local flow structure, turbulent exchange characteristics and do not take into account the structure of the lateral convective exchange caused by helical wire wraps on fuel pins. The subchannel method consist on solving a system of equations of mass, momentum and energy macrotransport (balance) written for elementary cells into which a subassembly is divided [14, 15]. The subchannel method allows us to find the thermohydraulic characteristics of coolant in the total volume of a subassembly by taking into account all the mechanisms of intercell interaction, as well as taking into account heat exchange between fuel subassemblies and to calculate the temperature field for the whole set of subassemblies of the fast reactor core. There are a lot of codes in which the subchannel method is realized as applied to hydrodynamically stabilized and non-stabilized flow conditions of coolant in fuel subassemblies. Their analysis is given in [14]. Drawback of these codes include the difficulty of taking into account the mechanisms of interchannel exchange in fuel subassemblies, the approximations required to take into account the distortion of fuel subassemblies and the difficulty of taking into account the longitudinal momentum and heat exchange.

Accordingly, the results of calculations by various codes appreciably differ even for the commonly used versions, such as COBRA-IV [16], COTEC [17], SUPERENERGY [18] and TEMP-M [19]. A complete and precise system of interchannel exchange coefficients was obtained as a result of systematic experimental studies carried out at IPPE (Obninsk) and was realized in the TEMP-M and MIF codes. Fig. 1.2.1.1. presents the comparison of calculations by various codes with the experimental data.

The porous body model describes the field of elementary unit-averaged coolant velocity and temperature values and is based on solving the equations of motion and energy with volumetric friction and energy release [21-23]. The porous body model only approximately takes into account the local flow structure, peculiarities of subassembly geometry (periphery,
deformations) and is effective at temperature field calculations in case of considerable flow disturbances, for example, of a partial solid flow blockage in a subassembly. The porous body model for a single-phase flow of coolant was realized, for example, in the codes UZU [25], COMMIX [26], GRIF [27].

Fig. 1.2.1.2. illustrates a satisfactory agreement between the results of calculation by the code UZU and experimental data for a partially blocked flow subassembly [25].

A number of codes were tested in out-of-pile experiments at partially blocked subassembly flow [28]. The main features of the codes tested are: SABRE and ASFRE are three-dimensional subchannel models; BACHHUS and CAFCA are the codes based on the porous body model. Within the frames of cooperation between KFK and UKAEA some experiments in sodium (49% central blockage and 21% corner blockage) were calculated by the code SABRE [29, 30]. Good agreement for the case of the central blockage and considerable discrepancy with the experiment for the corner blockage were obtained.

Within the frames of LMBWG a benchmark for some codes was carried out [31-34]. Table 1.2.1.1. presents informations on benchmark calculation of these codes. Only SABRE was tested in all experiments. Fig.1.2.1.3, 4. shows measured and calculated radial temperature profiles of different measuring levels. In addition to the codes participating in the benchmark in the figures the results of calculation by the GRIF code are also presented.
Fig. 1.2.1.2. Temperature rise distribution behind blockage:

a) along subchannels b) radial profiles of different levels

O, ●, □, ■, △- measured data
□□□□ - calculated UZU
1.2.1.2. Boiling condition

The analysis of transient coolant flow conditions in pin subassemblies has become especially urgent in connection with investigating transient operating conditions of reactors and the analysis of various emergency situations as well. The first models for calculating sodium boiling in the reactor channel were based on consideration of single bubble expansion [35-37]. This was explained both by sodium tendency to superheat which was initially described as very high and by a generally fast transition of sodium boiling conditions to slug, piston annular flow patterns. Further development of calculation models were carried out towards increasing the number of bubbles forming in the channel. In this case in modern models sodium superheating above the saturation temperature is, as a rule, taken to be no more than 20°C.

It should be noted that these models are in good agreement with the experimental data. Up-to-date codes for calculating the ULOF and UTOP type accidents- SAS 4A, SAS 3D, EAC-1, FRAX, CAPR-1, CARMEN [38-40] - most often represent cooling boiling using variants of well known multibubble models as SAS-2A [41] developed in USA and BLOW-3 [42] developed in Germany. Later on these models were developed to represent of liquid film motion, its break down by vapour flow and the pin surface dryout criterion. Most often in these models the initial thickness of liquid film is taken, on the base of the liquid fraction over the channel cross-section, as equal to 0.15, and dryout begins when 1/3 of its initial thickness remains. The first code
developed in Russia for fast reactor channel dynamics calculations, taking into account sodium boiling, was also based on the single-bubble model [43].

The main contents of up-to-date models are the system of mass energy and momentum equations for the two-phase non-equilibrium flow, the closing relations and corresponding boundary conditions (a delay in the development of such models for sodium is connected with large non-linearities and discontinuities of derivatives at the liquid-vapour interface, as for sodium the ratio between liquid density and vapour density is considerably greater for water; of great importance is also the relationship between the absolute pressure value of the medium and pressure drop in the channel).

A three-dimensional two-fluid model has been obtained by using temporal or statistical averaging. The model is expressed in terms of two sets of conservation equations governing the balance of mass, momentum and energy in each phase. However, since the averaged fields of one
<table>
<thead>
<tr>
<th>Experiments</th>
<th>Blockage</th>
<th>KfK</th>
<th>ECN</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Medium</td>
<td>KfK</td>
<td>KfK</td>
</tr>
<tr>
<td>Inlet temp. [°C]</td>
<td>Sodium</td>
<td>Central 49%</td>
<td>Corner 21%</td>
</tr>
<tr>
<td>Run No.</td>
<td>1</td>
<td>6</td>
<td>282</td>
</tr>
<tr>
<td>Flow velocity [m/s]</td>
<td>4</td>
<td>1</td>
<td>1</td>
</tr>
<tr>
<td>Leakage flow [%]</td>
<td>-</td>
<td>-</td>
<td>-</td>
</tr>
<tr>
<td>Codes and Calculations</td>
<td>ASFRE (PNC)</td>
<td>x</td>
<td>x</td>
</tr>
<tr>
<td>SABRE (UKAEA)</td>
<td>x</td>
<td>x</td>
<td>x</td>
</tr>
<tr>
<td>BACCHUS (KfK)</td>
<td>x</td>
<td></td>
<td></td>
</tr>
<tr>
<td>CAFCA (EdF)</td>
<td>x</td>
<td>x</td>
<td></td>
</tr>
<tr>
<td>MICHELLE (CNEN)</td>
<td>x</td>
<td></td>
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</tbody>
</table>

TABLE 1.2.1.1. BENCHMARK CALCULATIONS WITH LOCAL BLOCKAGES (9TH AND 10TH MEETING OF THE LMBWG)
phase are not independent of the other phase, the interaction terms appear in the field equations as source terms. For the most general dynamic problems such models were developed [44-46] and can reduced to the following form useful for reactor applications:

$$\frac{\partial (\phi_k \rho_k)}{\partial t} + \nabla (\phi_k \rho_k \vec{W}_k) + \nabla G_k = \Gamma_k$$  \hspace{1cm} (1)

$$\frac{\partial (\phi_k \rho_k \vec{W}_k)}{\partial t} + \nabla (\phi_k \rho_k \vec{W}_k \vec{W}_k) + \nabla \tau_k = \vec{W}_k \vec{M}_k + \vec{M}_k - \vec{M}_k$$  \hspace{1cm} (2)

$$\frac{\partial (\phi_k \rho_k \vec{h}_k)}{\partial t} + \nabla (\phi_k \rho_k \vec{h}_k \vec{W}_k) + \nabla q_k \vec{M}_k = \vec{h}_k \vec{M}_k + \vec{q}_k + q_k \vec{L}_s$$  \hspace{1cm} (3)

where $\Gamma_k$, $M_{ki}$, $\tau_i$, $q_{ki}$ and $\Phi_k$ are the mass generation, generalized interfacial drag, interfacial shear stress, interfacial heat flux and dissipation, respectively. The subscript $k$ denotes the "k"-th phase (1 or g), $i$ stands the value at the interface, $I/L_s$ -interfacial area per unit volume of the mixture. Other commonly accepted terms and definitions below are not specified.

The presented system of 3 equations calls for the introduction of four equations of state for each of the densities $\rho_k = \rho_k(p_k, T_k)$ and phase enthalpies $h_k = h_k(p_k, T_k)$. Each third term in the left hand side of the equations (1) - (3) is responsible for the effects of mass interchange at the surface of the isolated control volume. It allows us to use this system of equations for the subchannel reactor core analyses as well. For such subchannel analyses, as a rule, an additional momentum equation in the lateral direction, as well as pertinent modelling representations of the intercell mass transfer are required. In case of the subchannel effects being neglected, as well as at the process analysis in single-related configurations these terms should be omitted [47].

The interfacial transfer terms on the right-hand side of the equations (1) - (3) are obtained from the balance laws at the phase interface. It calls for conducting the local phase parameter averaging processes on the both sides of the interface. In so doing the interchange conditions for the mixture should be satisfied:

$$\sum_k \dot{M}_{ki} = 0 \hspace{1cm} (ii)$$

$$\sum_k \Gamma_k = 0,$$

$$\sum_k (h_k \Gamma_k + q_k / L_s) + 0$$

In addition to the initial and boundary conditions (which are not presented here), along with the terms of turbulent heat transfer ($q'_{ki}$) and momentum ($\tau'_{ki}$), constitutive equations are necessary for the interfacial transfer terms.
The interaction terms which couple the transport of mass, momentum and energy across the interface appear in the macroscopic field equations after using a proper averaging method. It can be said that much of the experimental information necessary to develop an accurate two-fluid model is not available. Therefore, the present state of the art in the two-phase flow instrumentation implies that considerable uncertainties exist in the constitutive relations for the two-fluid model. In spite of these shortcomings, however, there is no substitute available for modelling accurately two-phase phenomena where the two-phase are weakly coupled. Examples of these are [44]: sudden mixing of two phases; transient flooding and flow reversal; transient countercurrent flow, and two-phase flow with sudden acceleration.

It should be noted here that the presented system of equations (1) - (4) is not the only possible one nor is it a fully validated one. The derivation and investigation of the properties of the local equations of the conservation laws are currently being actively developed at many research centers.

If the model is logically self-consistent, i.e., compatible with the set of adjusting equations and sufficient for completing the system of equations, then this in principle provides a possibility of its solution. However, in practical studies, due to a high level generality of the system of equations (1) - (4) and a great number of constitutive relations, some simplifications are needed, in particular for obtaining numerical results.

Various mixture models are used extensively because of their simplicity, both in terms of the field equations and necessary constitutive relations. In view of the limited data base presently available and the difficulties associated with detailed measurements in two-phase flow, the advanced mixture models, such as the drift-flux model, are probably the most reliable and accurate analytical tools for ordinary two-phase flow problems.

For the analysis of transient and accidental conditions codes are used that differ by model dimensionality, by the method of representation of the closed system of equations (homogeneous or separate flow of phases) and by realization of the macrotransport equations solution in the multidimensional statement (subchannel model or porous body model).

For solving the fluid dynamic equations the ICE [48], SIMPLE [49] methods and their modifications are predominantly used.

In 1989 a benchmark was carried out in which calculations by the codes ESSO, NATREX, NASLIP and SABRE were compared with the results of experiments on stationary sodium boiling carried out at CCR Ispra laboratories. The results of comparison are presented in Figs. 1.2.1.5, 6. In addition, in these figures are presented the results of calculations by the Russian codes SOBOIL [50] and BODY [51].

The most developed multidimensional codes intended for the calculation of non-stationary sodium boiling conditions at flow blockages or at a decrease of flow rate through a subassembly are as follows: COBRA-IV (USA), BACCHUS-3D/ST/TP (Germany), BACCHUS-Eb (France), CAFCA-Na2 (France), SABENA-3D (Japan), SABRE-3C (UK). All codes, except the first one, have undergone testing in the experiments space on 37-pin subassembly at a decrease of sodium flow rate through it on the rig at KfK [52]. In Figs. 1.2.1.7-9 some results of comparison between calculations and experiments are presented. Summarized results of the benchmark exercise are presented in Table 1.2.1.1.

As a whole, the codes describe the experimental results sufficiently well. However, the "fine" effects, such as flow rate oscillations, are best described by the codes with two-liquid models. But it should be said that the calculations were carried out on already known experimental data.
Fig. 1.2.1.5. Boiling point as a function of sodium velocity at channel inlet

- BODY
- ESSO
- NATREX
- SABRE
+ SOBOIL
- experiment
Fig. 1.2.1.6. Pressure drop as a function of sodium velocity at a channel inlet

- BODY • - experiment ○ - NASLIP

Δ - ESSO + - SOBOIL
Fig. 1.2.1.7. Axial expansion of the boiling boundary

BACCHUS-Eb
BACCHUS-3D/SP/TP

*Fig. 1.2.1.8. Inlet and outlet flow velocity in test L22*

\[ \text{Power switch off} \]

**BACCHUS-3D/SP/TP**

*Fig. 1.2.1.9. Course of pressure in test L22*

**CONCLUSIONS**

1. There is considerable progress in the development of fast reactor thermohydraulic calculation methods, studies and activities in this subject are continued. Progress in the development of mathematical methods for thermohydraulic studies of reactor fuel subassemblies has been achieved mainly due to the establishment of an analogy between the classical equations of hydrodynamics and heat exchange, and equations of macrotransport in subchannel approach or within the frames of the porous body model.
At present for an analysis of accidental situations in liquid metal-cooled fuel subassemblies the following codes are mainly used: COBRA-IV, BACCHUS-3D/ST/TP, BACCHUS-Eb, CAFCA-Na2, SABENA-3D, SABRE-3C.

The comparison of calculated results with experimental data on coolant boiling in fuel subassemblies reveals their difference in some situations. It is necessary to carry out experimental and analytical-theoretical studies of flow structure and processes in liquid metal-cooled fuel subassemblies at coolant boiling, to obtain closing relations and to carry out further development of numerical models and methods for solving non-stationary problems of heat exchange with coolant boiling in two- or three-dimensions.

The codes developed should form a part of the complex program of physical, thermohydraulic and mechanical calculations.

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1.2.2. Methods and codes for molten fuel-sodium interaction analysis

1.2.2.1. Mathematical simulation of molten fuel-coolant interaction

The earliest estimates of the work potential of an interaction between molten fuel and sodium was made by Hicks and Menzies [1]. In this treatment the heat was transferred instantaneously and the mixture expanded adiabatically so that only thermodynamic considerations were necessary. It is possible to determine the peak pressure and the potential work available on expansion down to say 1,0 atmosphere but the pressure time history is not defined. For the latter a hydrodynamical model is required and this was first attempted by Padilla [2] who assumed that the region in which the interaction took place expanded against the acoustical constraint of a column of sodium. In this case the pressure time history depended on the rate at which energy was transferred from the molten fuel to the sodium. This work indicated that the mechanical work could be significantly less than the thermodynamic limits and depended on the heat transfer time relative to the expansion time of the interaction zone. Furthermore, if it were assumed that when vapour was formed in the interaction zone it inhibited heat transfer, this could reduce the work potential to only a small fraction of the thermodynamic yield. For rapid fragmentation and mixing, sodium columns of reactor dimensions and interaction zones longer than a few cms the coolant in the interaction zone remains in the liquid state due to the high pressures and a shock wave is propagated to the upper surface. The returning rarefaction wave will cause vaporization of the coolant when it arrives back at the interaction zone. Cho, Ivins and Wright [3, 4] extended this type of analysis and introduced three different heat transfer approximations which were intended to represent in a simple way: mixing, fragmentation and heat conduction from the fuel to the sodium. In analytical models, as a rule, "fine" mechanisms of fuel fragmentation and its mixing with coolant are not considered and it is taken into account parametrically. In the general form the analytical model of the molten fuel-coolant interaction can be presented as follows. There is considered some reaction volume V including fuel, coolant and non-condensable gases.

The basic equations governing the thermo-hydraulic of the MFCI problem are as follows:

\[
\frac{d(M_{Na}I)}{dt} = \frac{dQ}{dt} + V_{Na} \frac{dP}{dt}
\]

an equation of the first law of thermodynamics (1)

\[I = f(P, V_{Na})\]

an equation of state (2)

\[C_f \rho_f \frac{dT_f}{dt} = \lambda_f \Delta T_f + Q_f\]

an equation of heat conduction in fuel (3)

\[PV_g^n = P_0 V_g^n\]

an equation of polytropic process for gas (4)

\[P = f(V, \frac{dV}{dt}, \frac{d^2V}{dt^2})\]

an equation of coolant moving (mechanical constraint) (5)

These equations, in combination with intial and boundary conditions, represent a closed system. The equation of state for sodium in a one-phase flow is written as:

\[
\frac{dV_{Na}}{dt} = -\beta V_{Na} \frac{dP}{dt} + \alpha V_{Na} \frac{dT_{Na}}{dt}
\]

an equation of the state of sodium in a one-phase flow (6)
where $\alpha$ - the thermal expansion coefficient

$\beta$ - the isothermal compressibility of sodium

In the general case, masses of fuel, sodium and gas are variable quantities.

In literature there have been presented many models used for MFCI calculations [3-23]. Here we shall not dwell in detail upon each model, their characteristic features being presented in Table 1.2.2.1. The most differences in models developed by many authors are related with the determination of the heat flux $dQ/dt$ to sodium and the solution of the equation of the mechanical constraint. These two processes may be described most fully by considering MFCI in two phases.

During phase A following a fuel contact with sodium and its subsequent fragmentation, rapid heating of sodium and pressure rise take place. Then sodium is considered as a compressible medium through which the acoustic wave is propagated. After its reflection from the free surface it returns as a rarefaction wave. If $z(t)$ is the time-dependent height of the interaction zone from a reference level equation (5) during this phase of the process can be written in the acoustic approximation.

$$\frac{dz}{dt} = \frac{P(t) - P_0}{\rho_{Na} C_o}$$  \hspace{1cm} (7)

where $P_0$, $\rho_{Na}$, $C_o$ are the initial pressure, density and sound velocity in the sodium column, respectively.

After the acoustic relaxation time period, sodium compressibility may be neglected and an inertia approximation (Newton’s law) can be used. The pressure rises, reaches a maximum and then falls as soon as the expansion of the liquid sodium becomes important. At the time at which the pressure reaches the saturation point, sodium boiling starts and phase B begins. During phase A, the heat exchange of fuel particles with sodium is mainly determined by the thermal resistance of fuel as heat conduction of sodium in considerably greater than heat conduction of uranium dioxide (or plutonium). For other fuel types this approximation introduces a certain error. In most models it is assumed that sodium is heated uniformly without considerable temperature gradients. For the calculation of the heat flux from fuel to coolant various approximations are used: from simple to complex based upon the solution of the equation of heat conduction in fuel particles.

The greatest difficulties arised solving the thermal part of the problem during phase B when sodium boils. In some models it is assumed complete heat transfer cut off whenever any vapor is presented, in other ones, on the contrary, a sodium film is left on the particles surface which ensures good heat transfer. In other models the heat transfer surface and heat conduction of sodium are varied according to volume vapour content variation.

In models [13, 14] the heat transfer coefficient from the surface of a spherical particle of fuel at sodium boiling up is assumed equal to 0.2 and 0.1 W/cm$^2$°C, respectively.

Caldarola [7, 8] has introduced a more complex heat transfer process which is based on some experimental results of the cooling of hot sphere surrounded by subcooled liquid. It is assumed that the particle remains suspended inside the vapour bubble with the weigh of the particle being balanced by the force generated by the vapour flowing underneath the particle along the space between the particle and the liquid. The liquid is heated by conduction through the vapour layer and also by direct radiation.
<table>
<thead>
<tr>
<th>Name of Model/Author/Reference</th>
<th>Fragmentation and Mixing</th>
<th>Heat Transfer</th>
<th>Hydrodynamics</th>
</tr>
</thead>
<tbody>
<tr>
<td>Argonne Parametric Model I</td>
<td>Particle size distribution assumed. Fragmentation and mixing time can be simulated by choice of heat transfer mechanism</td>
<td>Heat transfer into liquid sodium. Options:- i Linear gradient transient conduction ii Quasi steady state iii Heat transfer with finite fragmentation and mixing rate</td>
<td>Acoustic and inertial constraint change-over on return of rarefaction wave advocated</td>
</tr>
<tr>
<td>Cho, Ivins and Wright [3, 4]</td>
<td></td>
<td></td>
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</tr>
<tr>
<td>TEXAS Antonakas [10]</td>
<td>Variable mass of fuel simulates fragmentation time [ M(t) = M \frac{1 - r}{1 - r} ]</td>
<td>Transient conduction for Stage A. Quasi Steady state for Stage B. 4 options on contact area</td>
<td>Inertial constrain throughout</td>
</tr>
<tr>
<td>Karlsruhe Model II Caldarola [7, 8]</td>
<td>Log normal distribution of particle size. Variable masses of fuel and sodium simulates fragmentation and mixing time.</td>
<td>When no vapour present, transient heat conduction. When vapour present a detailed model determines film thickness</td>
<td>Acoustic and inertial constraint change-over when vapour formed</td>
</tr>
<tr>
<td>Name of Model/Author/Reference</td>
<td>Fragmentation and Mixing</td>
<td>Heat Transfer</td>
<td>Hydrodynamics</td>
</tr>
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<td>-------------------------------</td>
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<tr>
<td>BRENDY I/II</td>
<td>BRENDY. No fragmentation but conductivities for fuel and sodium are increased to simulate increased area of fragmentation MURTI. Interactions starting successively in each of several sections with time delay</td>
<td>One dimensional conduction from fuel layer to sodium with effective conductivity</td>
<td>BRENDY I - One dimensional Lagrangian hydrodynamics</td>
</tr>
<tr>
<td>MURTI</td>
<td></td>
<td></td>
<td>BRENDY II - Acoustic inertial constrain</td>
</tr>
<tr>
<td>Jacobs [5]</td>
<td></td>
<td></td>
<td>MURTI - Acoustic and then inertial constrain with one dimensional Lagrangian hydrodynamics in the interaction zone</td>
</tr>
<tr>
<td>FUS-REC 1/2</td>
<td>Single particle size assumed Variable fuel mass interacting</td>
<td>Phase A pure conduction into liquid using polynomial type approximation. Phase B heat transfer into mixture of liquid and vapour, coeff varies with void fraction. (FUS-PEC 2 solidification of UO₂ taken into account)</td>
<td>Acoustic constrain with inertial constrain after return of rarefaction wave</td>
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<td>Martini and Scarano [6]</td>
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<tr>
<td>Name of Model/Author/Reference</td>
<td>Fragmentation and Mixing</td>
<td>Heat Transfer</td>
<td>Hydrodynamics</td>
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</tr>
<tr>
<td>CORFOU Syrmalenios [9]</td>
<td>Distinguishes between fragmentation (large scale break up) and dispersion (small scale break up) on different time scales</td>
<td>Heat is transferred from fuel to liquid sodium in the interacting region. There is exchange of mass and heat between vapour and liquid which are not in equilibrium. Additional liquid coolant can enter the interacting zone</td>
<td>Non-interacting liquid is taken as incompressible giving inertial loading</td>
</tr>
<tr>
<td>VS-4 Putten, Konig, Bogaard [11]</td>
<td>Fuel particles divided into groups which start their contribution at different times and axial coordinates with different particles radii</td>
<td></td>
<td>Hydrodynamic equation solved by method of characteristics</td>
</tr>
<tr>
<td>ISPRA-FCI Goldamper, Kottowsky [12]</td>
<td>Sodium column drops on fuel making intimate contact. Area of contact is related to kinetic energy of columns and increases continuously during acoustic travel time</td>
<td>Heat conduction to sodium during contact. Heat conduction through vapour space after return of rarefaction wave</td>
<td>Inertial constrain</td>
</tr>
<tr>
<td>Name of Model/Author/Reference</td>
<td>Fragmentation and Mixing</td>
<td>Heat Transfer</td>
<td>Hydrodynamics</td>
</tr>
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</tr>
<tr>
<td>SUGAR- PISCES</td>
<td>Taken into account at heat conduction determination</td>
<td>Heat transfer coefficient: in phase B - 1 W/cm² °C, completely vaporized sodium - 0.2 W/cm² °C</td>
<td>Two dimensional Lagrangian model</td>
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<td>SUGAR- CAMEL</td>
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<td></td>
<td></td>
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<tr>
<td>Suzuki [13]</td>
<td></td>
<td></td>
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<tr>
<td>FCI - II/ ASPRIN - II</td>
<td>Fragmentation by the exponential law</td>
<td>Transient conduction in phase A. Quasi-steady in phase B with the heat transfer coefficient from particle surface</td>
<td>One-dimensional compressible in Lagrangian coordinates</td>
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<tr>
<td>Savada [14]</td>
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<tr>
<td>LOCAL</td>
<td>Variable mass of fuel and sodium</td>
<td>It is similar to the Caldarola model</td>
<td>Acoustic and inertial constrain change-over on return of rarefaction wave advocated. LOCAL- fuel subassembly geometry, INTERACT- reactor geometry</td>
</tr>
<tr>
<td>INTERACT</td>
<td></td>
<td></td>
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<tr>
<td>Buksha [17, 18]</td>
<td></td>
<td></td>
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<tr>
<td>Berthoud [19]</td>
<td>Variable mass of fuel and sodium</td>
<td>Transient conduction</td>
<td>Inertial constrain</td>
</tr>
<tr>
<td>Doshi [20]</td>
<td>Log normal distribution of particle sizes</td>
<td>Caldarola model</td>
<td>Acoustic and inertial constrain change-over on return of rarefaction wave advocated</td>
</tr>
</tbody>
</table>
In Fig. 1.2.2.1 the interaction model is schematically presented. The models developed for the calculation of the molten fuel-sodium interaction, both in a single fuel subassembly and in the core as a whole are presented in [17, 18]. The thermal part of the problem is based on solving the equation of heat conduction in spherical particles (variable mass), taking into account the vapour film on their surface in phase B and vapour condensation upon cold surfaces, is the interaction zone expands.

A coherent thermo- and hydrodynamic model to explain MFCI in the CORRECT II experimental facility has been developed [19]. The effects of heat losses to the surroundings, mass entrainment, two-phase frictional losses and non-condensable gases are taken into account for the coolant modelization, while a solidification controlled fragmentation model is proposed for the fuel behaviour. Interpretations of two significant experiments in the CORRECT II facility permitted verification of the consistency of the formulation for the behaviour of the fuel and coolant.

1.2.2.2. Codes description and calculations results

Table 1.2.2.1 summarizes data on some codes developed in the world and on their features. At present there is insufficient experimental data for the determination of all necessary input parameters to obtain assured calculations. In Fig.1.2.2.2-1.2.2.5 the effect of the fuel particles size, of their surface heat transfer at sodium boiling, the time of fuel fragmentation and

![Diagram showing the location of the fuel particles, of the liquid sodium and of sodium vapour inside the reaction volume.](image)
its mixing with sodium of the interacting sodium mass on the pressure peak is shown [14]. Besides the above-parameters, the analytical results are greatly affected by initial conditions connected with the presence of vapour or gas in the interaction zone. Vapour and gas have a damping effect on the process and prevent the origination of the shock wave in the initial period of the process. Of course, rig and reactor studies, mainly in TREAT, SCARABEE, CABRI, have allowed us to compare calculations with experiments; however, up to now the accident calculations are carried out with rather conservative assumptions and large margins.

\[
t_m = 20\text{msec} \\
htwo = 0.1 \text{ w/cm}^2 \cdot ^\circ\text{C} \\
M_f/M_s = 9.52 \\
M_f = 1000\text{Kg}
\]

**Fig. 1.2.2.2.a Effect of the fuel particle radius \( R_f \) on two-phase peak pressure**
In [21, 22, 23] there are compared transient overpower accident calculations by well-known codes: SAS3D, SAS4A, FRAX, SURDYN, PHYSURA, CARMEN, CAPRI, EAC, etc. (here their main abbreviations are presented as a rule, they have some supplements taking account of their modifications). Below main MFCI modelling aspects and parameters used in some codes, participated in comparative analytical exercises are shortly described [22]. Main MFCI and channel geometry parameters are presented in Table 1.2.2.2.

In CARMEN/MAXICO, an initial mass of fuel is put into instant contact with the sodium adjacent to the failure in order to initiate the MFCI. The calculation of the fuel ejected subsequently is made by MAXICO in which the fuel pin meshes concerned and the time period during which the mass of fuel is linearly ejected into the coolant are fixed by input parameters. In all the other codes, ejection is more gradual. Besides the driving pressure gradient between the molten fuel cavity and the sodium channel (MFCI zone), the ejection rates are linked to the ejection model and the inertia of the fuel flow inside the cavity. Whilst PLUTO-2 and EPIC properly account for the transient flow inside the cavity and always eject fuel with equilibration of pressures on both sides of the failure rip, PHYSURA and SAS-3D/FCI use an instationary Bernoulli equation, for which inertia of fuel flow inside the homogeneous cavity is approximately represented by an inertial length. SAS-3D/FCI uses the maximum distance between failure rip and the axial extremities of the cavity, which amounts to approximately 0.60 m.

In the case of PHYSURA, the cavity lengths above and below the failure location are treated separately and the inertial lengths are obtained when integrating the continuity equations for liquid fuel with the assumption of a linear variation of velocity between cavity ends and
failure location. The hot molten fuel ejected from the pin through the clad rip fragments into small particles leading to a fuel-coolant interaction and to rapid voiding of the channel.

In CARMEN/MAXICO and SAS-3D/FCI, a given time is allowed for mixing and fragmentation of the fuel ejected into liquid sodium whereas in other codes it is instantaneous.

For SAS-4A/PLUTO 2, this is the case as long as the sodium in front of the failure is liquid. The fuel ejected into two-phase or voided regions is treated as annular or bubbly flow which can freeze on the cladding. This fuel does not heat efficiently the remaining high void fraction sodium.

![Diagram showing pressure transient over time with different mixing time constants](image)

Fig. 1.2.2.4. Effect of the fragmentation and mixing time constant, $t_m$, on pressure transient
Fig. 1.2.2.5. Effect of sodium mass on peak pressure

- $K_f = 117 \mu$
- $t_m = 20$ msec
- $h_{two} = 0.1 \text{ W/cm}^2\cdot\text{oC}$
- $M_f = 1000 \text{Kg}$
### TABLE 1.2.2.2. MAIN FCI CHANNEL GEOMETRY PARAMETERS

<table>
<thead>
<tr>
<th>Parameter</th>
<th>SAS-30/FCI</th>
<th>SAS-4A/PLUTO 2</th>
<th>SAS-3D/EPIC</th>
<th>FRAX-3/EPIC</th>
<th>EAC/EPIC</th>
<th>SUROYN/PHYSURA</th>
<th>CARMEN/MAXICO</th>
</tr>
</thead>
<tbody>
<tr>
<td>Mass ratio evolution with time</td>
<td>variable</td>
<td>variable</td>
<td>variable</td>
<td>variable</td>
<td>variable</td>
<td>variable</td>
<td>constant</td>
</tr>
<tr>
<td>Mixing and fragmentation time s</td>
<td>10 ms</td>
<td>0</td>
<td>0</td>
<td>0</td>
<td>0</td>
<td>0</td>
<td>10 ms</td>
</tr>
<tr>
<td>Particle size μm</td>
<td>250</td>
<td>100</td>
<td>100</td>
<td>250</td>
<td>100</td>
<td>100</td>
<td>250</td>
</tr>
<tr>
<td>Effective fuel-sodium heat $10^4$W/kgK</td>
<td>20.2</td>
<td>3.57</td>
<td>3.57</td>
<td>(=19.75)</td>
<td>3.50</td>
<td>3.67</td>
<td>1.48</td>
</tr>
<tr>
<td>Na vapour condensation coefficient in FCI zone $10^4$W/m$^3$K</td>
<td>yes</td>
<td>yes</td>
<td>yes</td>
<td>yes</td>
<td>yes</td>
<td>yes</td>
<td>yes</td>
</tr>
<tr>
<td>Total channel length m</td>
<td>3.50</td>
<td>3.52</td>
<td>3.52</td>
<td>5.00</td>
<td>3.50</td>
<td>3.40</td>
<td>3.20</td>
</tr>
<tr>
<td>Upper slug length m</td>
<td>1.18</td>
<td>1.18</td>
<td>1.18</td>
<td>2.25</td>
<td>1.25</td>
<td>1.40</td>
<td>1.20</td>
</tr>
<tr>
<td>Lower slug length m</td>
<td>2.32</td>
<td>2.34</td>
<td>2.34</td>
<td>2.75</td>
<td>2.25</td>
<td>2.00</td>
<td>2.00</td>
</tr>
<tr>
<td>Representation of lower orifice</td>
<td>yes</td>
<td>yes</td>
<td>yes</td>
<td>no</td>
<td>no</td>
<td>no</td>
<td>no</td>
</tr>
<tr>
<td>Length of clad rip m</td>
<td>0.10</td>
<td>0.10</td>
<td>0.074</td>
<td>0.10</td>
<td>0.10</td>
<td>0.10</td>
<td>0.10</td>
</tr>
</tbody>
</table>
In CARMEN/MAXICO, only the molten mass in the fuel node in front of the clad rip interacts. It is initially put directly into contact with the sodium. Fragmentation and heat transfer follow a Cho-Wright model.

In all codes, heat transfer from fuel to sodium is proportional to the liquid fraction of sodium.

In SAS-3D/FCI, CARMEN/MAXICO and SURDYN/PHYSURA, the MFCI zone is treated as homogeneous whereas SAS-4A/PLUTO 2 and the EPIC-based calculations have a heterogeneous treatment of this zone, allowing for the build-up of pressure and density gradients.

In SAS-3D/FCI, CARMEN/MAXICO, SURDYN/PHYSURA and SAS-4A/PLUTO 2 the retreating MFCI zone interfaces leave a liquid sodium film behind. In SURDYN/PHYSURA and SAS-3D/FCI, the film is then mixed with the MFCI zone two-phase sodium, thus reducing the average enthalpy. In SAS-4A/PLUTO 2, the film is allowed to be torn off by moving fuel. In CARMEN/MAXICO the film is stationary.

All codes except SURDYN/PHYSURA and CARMEN/MAXICO allow for recondensation of sodium vapour on the cold clad surfaces uncovered by the retreating MFCI interfaces. Table 1.2.2.2 gives the main parameters used for MFCI in the different calculations.

Three codes, SAS-3D/FCI, FRAX-3/EPIC and CARMEN/MAXICO have assumed that the fuel fragments into 250 μm particles while the other codes assume 100 μm particles.

The effective fuel-to-sodium heat transfer coefficient depends primarily on particle size as all codes use the same formula in \( k/r \) (or its equivalent \( 3k/p\rho^2 \)) where \( k \) is the fuel heat conductivity, \( r \) the particle radius and \( \rho \) the molten fuel specific mass. In SURDYN/PHYSURA, however, the value was set by input.

The comparative results of the calculations are presented in Table 1.2.2.3. As is seen from this table spread in maximum pressure values obtained from the calculation was as follows: from 90 atm to 150 atm (single phase sodium) and from 50 to 240 atm (two-phase sodium). Up-to-date versions of these programs use the above MFCI models with the input data obtained from experimental studies.

In comparative analysis of loss-of-flow accident [23] in SAS-3D/FCI, the interaction zone is treated in a bulk sense with average sodium temperature, density and vapour pressure. An average fuel temperature and density is also used in the MFCI calculation in which the heat transfer is controlled by the fuel droplet radius, the fuel conductivity and the sodium vapour and fission gas void fraction (Cho-Wright model). The liquid sodium columns above and below the interaction zone are treated as incompressible slugs. This is also done in the models which are described later. However, these models have a simplified acoustic approach for the first few milliseconds after pin failure. Conceptual limitations of SAS/FCI are that it cannot eject fuel into an initially voided coolant channel and that no axial pin failure propagation can be treated. A certain level of validation of SAS/FCI model has been achieved through the analyses of CABRI TOP experiments [24]. In this analysis the calculated later dispersal of the channel fuel (20-30 msec after pin failure) is considerably (2-3) slower than experimentally observed. The EPIC model [23] is part of the SAS-3D/NRC whole-core accident code of the US NRC. SAS-3D/NRC is different from US DOE code SAS-3D in so far as EPIC has replaced SAS/FCI and has been included in FRAX-4. The numerical treatment of EPIC is considerably more refined than that of SAS/FCI. In the coolant channel a one-dimensional two-fluid finite difference calculation is performed. The mixture of sodium droplets, sodium vapour and fission gas is the one fluid, the fuel particles the other. The heat transfer between the particles and the sodium is also based on
TABLE 1.2.2.3. BOEC COMPARATIVE RESULTS, POST-FAILURE FCI CONDITIONS

<table>
<thead>
<tr>
<th></th>
<th>SAS-3D/FCI</th>
<th>SAS-4A/PLUTO 2</th>
<th>SUROYN / PHYSURA</th>
<th>CARMEN</th>
<th>SAS-3D/EPIC</th>
<th>EAC/EPIC</th>
<th>FRAX/EPIC</th>
</tr>
</thead>
<tbody>
<tr>
<td><strong>START OF FCI</strong></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>FCI fuel temp. °C</td>
<td>3386</td>
<td>3173 °C</td>
<td>3215</td>
<td>3307</td>
<td>3406</td>
<td>3445</td>
<td>3122</td>
</tr>
<tr>
<td>Coolant temp. °C</td>
<td>838</td>
<td>783</td>
<td>795</td>
<td>809</td>
<td>846</td>
<td>844; 877</td>
<td></td>
</tr>
<tr>
<td>Fuel mass g or</td>
<td>2.0*</td>
<td>0.0</td>
<td></td>
<td>11.4</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Fuel ejection rate g/s</td>
<td>1900</td>
<td>1250</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Maximum pressure MPa</td>
<td>9.13'</td>
<td>24</td>
<td>15.3</td>
<td>1.512</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Duration ms</td>
<td>10.4</td>
<td>3.5</td>
<td>3.8</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Fuel/Na ratio at end.</td>
<td>5.15</td>
<td>3.5</td>
<td>3.5</td>
<td>6</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Maximum Na vapour MPa pressure</td>
<td>6.4</td>
<td>6.</td>
<td>19.0</td>
<td>9.5</td>
<td>24</td>
<td></td>
<td>4.9</td>
</tr>
<tr>
<td><strong>TWO PHASE</strong></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Upper slug at top of upper breeder at time ms</td>
<td>7</td>
<td>16</td>
<td>12.0</td>
<td>~18</td>
<td>8.4</td>
<td>10.</td>
<td>21.2</td>
</tr>
<tr>
<td>Lower slug at bottom of low breeder at time ms</td>
<td>never leaves</td>
<td>never reached</td>
<td>31.5</td>
<td>~38</td>
<td>51.0</td>
<td>43.0</td>
<td>64.1</td>
</tr>
</tbody>
</table>

* solid fuel
the Cho-Wright approach. However, due to the finite difference approach taken, it is a multiple interaction zone treatment and not a single uniform treatment as in SAS/FCI. PLUTO 2 is the fuel motion and sodium voiding model for TOP conditions in the US DOE code SAS 4A [23]. It considers the basic physical assumptions of SAS/FCI and axial cladding rupture propagation as in EPIC. With regard to the injection into voided channels, PLUTO 2 considers partially annular, fully annular and bubbly fuel flow regimes, as well as fuel freezing and plateout.

LEVITATE is the fuel motion model for fully or partially voided coolant channels in code SAS 4A [23]. In the disrupted region and the voided coolant channels a one-dimensional three-fluid treatment is used.

The more recent codes use multi-phase component thermal hydraulic models and are validated on out-of-pile and in-pile experiments. In [25] there are some details concerning MFCI treatment in FRAX5/EPIC, PAPAS2S, SAS3D, SAS4A, CASAS and PHYSURAC codes.

In restrained conditions, all models assume that when fuel is injected into the channel, fuel/sodium mixing occurs and a fine fuel fragmentation (fragment size of 50 to 200 μm, depending in some codes on coolant temperature) leads to a fast heat transfer from fuel to sodium, which decreases with increasing vapour volume. The fuel fragmentation time constant is about one ms or less. The heat transfer is cut-off either beyond some void fraction threshold (in PHYSURAC) or some partial vapour pressure threshold (in FRAX5/EPIC), or is weighted by a decreasing function of void fraction in the codes.

In semi-restrained conditions, where sodium boiling has already occurred before pin failure, phenomena are modelled differently within codes, depending whether pin failure occurs into single phase or two-phase sodium. SAS3D and CASAS use one-node description, which may lead to some difficulty in reproducing the experimental data, whereas FRAX5/EPIC, SAS4A and PAPAS2S take into account the local void fraction by an axial meshing. Codes using an axial meshing of fuel/sodium mixing zone, and also to some extent, PHYSURASC code using a specific treatment in semi-restrained channel cases, get a better agreement in the axially asymmetric situations in semi-restrained channel cases, than one point models.

During a hypothetical core disruptive accident (HCDA), a fast reactor vessel would be pressurized principally as a result of sodium vapour generated by heat transfer from degraded fuel. Knowles [26] distinguishes two alternative vapour production mechanisms: Q*-event and MFCI. In the latter, a very specific structure for the initial mixture of molten urania and coolant, is necessary to enable a propagating event. The slower contact-rate of melt and liquid coolant, and larger debris particles, renders Q*-events less energetic than MFCI for the same quantity of involved melt (see more details of these problems in Chapter 1.1.4).

Q*- and MFCI events exhibit three characteristics that are imperative constituents of any simulation. Firstly, marked thermal disequilibrium exists in the coolant during Q*- and MFCI events. Secondly, calculations of the mechanical yield on the basis of an isentropic vapour-expansion consistently overpredict measurements by at least six times. Accordingly, a strong thermodynamic irreversibility must alleviate Q*- and MFCI events. Thirdly, experiments at the Stanford Research Institute and Winfrith demonstrate that pressurization of the cover gas is much greater when effected by bubbles of compressed permanent gases than vapour bubbles of the same intrinsic energy. Because fluid friction is greater in the more energetic expansions of gas bubbles, it is concluded that interfacial condensation is the dominant irreversibility that saps energy from an expanding vapour bubble.

To predict the thermal efficiencies of MFCI and Q*-events at rig- and reactor- scales the BUBEX (Bubble Expansion) code has been developed. It represents to some extent the significant
thermodynamic disequilibrium and irreversibilities that obtain; particularly the alleviating influence of interfacial condensation. Principal uncertainties include: the quantification of the mass flux entrained by Rayleigh-Taylor instabilities, the size statistics of these droplets and the behaviour of the condensation coefficient at above 1 bar pressure. Droplets entrained within an HCDA bubble have a crucial influence on its dynamics in terms of their additional interfacial area for condensation and their aerosol scrubbing capability. Further specific experiments, and possibly theoretical investigations, are considered necessary therefore to improve confidence in BUBEX or any other model of bubble behaviour.

Results are presented of a BUBEX simulation for the sodium-urania MFCI experiment SUS01 in the Molten Fuel Test Facility at Winfrith. Excellent agreement between the predicted and experimental recorded pressurizations of the cover gas is achieved by tuning the one undefined parameter of the model that relates to droplet size statistics. In this simulation, the hydrodynamics of the liquid pool are characterized by 1-D model bases on Rayleigh's analysis, but with an intuitively reasonable allowance for the finite reactor geometry. Despite the encouragingly good agreement, the same hydraulic model did not adequately reproduce experimental results for the expansion of a perfect gas bubble. Although a rectilinear 1-D hydraulics model simulates adequately the expansion of permanent gas bubble and it imposes an initially over-large inertial loading which causes the premature collapse of the SUS01 vapour bubble by condensation. Neither 1-D hydrodynamic model can therefore be validated, so that at least a 2-D representation is considered necessary for MFCI simulations. Substantial progress has been made towards incorporating BUBEX into the comprehensively validated 2-D SEURBNUK hydrodynamics code.

CONCLUSIONS

The molten fuel-coolant interaction process plays an important part in severe accident analysis. In the world a number of models and codes for the molten fuel-sodium interaction analysis have been developed. At present there is insufficient experimental data to correctly take into account in analytical models various aspects of physical phenomena accompanying the interaction process. The results of calculations are sensitive to input data including as follows: the particle size; the time of fuel fragmentation and its mixing with coolant; particles-to-coolant heat transfer conditions; the amount of fuel and coolant effectively participating in the interaction process; geometrical and thermal characteristics of structures being in the interaction zone; thermal properties of sodium at high temperatures, etc. Further research should be directed to the development of models taking account of: the fuel particles spectrum; three-dimensional geometry; non-equilibrium character of the liquid-vapour system; the mechanical behaviour of the reactor vessel and in-vessel equipment, etc. It is essential to develop models comprising the thermal interaction of a multicomponent system (sodium, fuel, steel being in various aggregate states). Analytical models of molten fuel-coolant interaction should enter as modules into complex codes for calculating the accidents leading to core melting.

REFERENCES


1.2.3. Decay heat removal analysis

In analytical models the degraded core debris are considered, as a rule, as a heat generating bed of particles submerged in liquid sodium on a horizontal surface. As was already mentioned in Chapter 1.1.5, heat removal from the bed is effected due to heat conduction and natural or forced circulation. Significant cooling can be achieved by boiling within the debris. However, under some conditions, the vapor vaporization rate will exceed the replenishing rate of inflowing liquid, and a portion of the bed will become dry. Because of the low thermal conductivity of dry debris, the dry zone can achieve high temperatures and threaten support or containment structures. Thus conditions that introduce dryout within post accident debris mark a domain of greatly reduced coolability.

The most important physical parameter determining the capability of heat removal from the bed is the heat flux (heat power) at which the dryout occurs. Until recently it has been accepted that incipient dryout is a condition of heat removal impossibility. However, in-pile experiments (see Chapter 1.1.5) have shown that even in a dried-out state the bed can exist rather long if heat removal conditions permit it.

Most analytical researches are directed to obtaining the dependencies of critical heat fluxes on various parameters of the bed and heat transfer conditions. As a rule, they are based upon the analysis of direct experimental data or have empirical coefficients obtained from the known physical phenomena.

1.2.3.1. Critical fluxes

A descriptive picture of the process leading to dryout can be presented in a general form as follows (Fig. 1.2.3.1.). In a particle bed submerged in coolant natural circulation

![Diagram of boiling and dryout in a bed of particles debris](image)

*Fig. 1.2.3.1. Schematic view of boiling and dryout in a bed of particles debris*
is developed (in case the plate underlying the bed is perforated and sources of forced circulation are present then, naturally, this circumstance should be taken into account) where coolant under the action of gravitational and capillary forces goes downwards. Vapour generated at boiling goes upwards creating a pressure gradient. With a large vapour production rate a large pressure develops in the vapour at the bed bottom (due to the resistance to vapour flow). This large pressure may prevent liquid from reaching the bed bottom and then dryout occurs. If the bed is bottom cooled then at the bed bottom can be present a region of liquid coolant at the boundary of which with the boiling zone there is also heat exchange due to drawn up the liquid into the bed by capillary force. There is one more circumstance which influences the critical flux value to a considerable degree. So far we have been speaking about boiling in a tightly packed particle bed. The particles will remain in place if there is enough pressure on them from the weight of the bed above them. Near the top of the bed, there is not a sufficient pressure from the weight of the bed and the vapour may push back both the liquid and the particles. In this fashion, channels are formed. The formation of channels allows the orderly flow of liquid going downward between the channels and vapour going upward within the channels. Because of the greatly reduced flow resistance within channels, they can have a strong effect on heat removal through the debris if their length is now a negligible fraction of the debris thickness. The conditions of incipient dryout are changed, as a result of which the value of the critical flux is increased. The character of the process is also affected by the particle size, which determines the flow conditions, and particle stratification, where larger particles are accumulated at the bed bottom and fine particles at the top, which results in a complex distribution of the capillary forces and hinders liquid penetration deep into the bed. Analytical correlations of critical fluxes in porous beds built up of particles are described in a number of papers [1-19]. Here let us dwell upon some of them.

Sowa et al. [1] presented the first dryout model that was based on flooding correlations from the chemical industry. However, these correlations apply to large particles (~ 10 mm) whereas for our conditions particles of 0.02-1 mm are required.

Their relation for the critical flux is as follows:

\[ q_d = 0.0802 \ h_{lv} \left( \frac{\rho_v \rho_l \ g \ d \ \epsilon^3}{(1-\epsilon) (\rho_v/\rho_l)^{1/4}} \right)^{1/2} \left( \frac{\mu_v}{\mu_w} \right)^{0.1} \]  

where \( h_{lv} \) - heat of vaporization,
\( \rho_v, \rho_l \) - vapor density and liquid density for saturated liquid water,
\( d \) - particle diameter,
\( \epsilon \) - porosity,
\( g \) - gravitational acceleration,
\( \mu_v \) - the dynamic viscosity for saturated liquid water at atmospheric pressure,
\( \mu_w \) - dynamic viscosity of the liquid coolant.

As is seen from the above relation the dryout flux increases with the square root of particle diameter and is independent of the bed thickness.

Gabor et al. [2] developed a relation of the dryout flux for a Na-UO\(_2\) bed:

\[ q_d = 1050000 \ [W/m^2] \text{ for } c < 433 \]
\[ q = 3460000 - 7130 \ c + 4.02 \ c^2 \ [W/m^2] \text{ for } c > 433 \]
where \( c \) is the bed loading, kg/m\(^2\).

This equation is valid only for non-subcooled sodium with the particle size distribution from 0.1 mm to 1.0 mm and bed porosity from 50 to 55%.

As is seen from the expression the critical flux is strongly dependent on bed thickness in contrast to equation (1).

Dhir et al. [3] developed a model based on their experiments. It applies to packed deep beds.

\[
q_d = \frac{0.0177 \rho_1 (\rho_1 - \rho_v) g d^2 \varepsilon^3 h_{lv} }{180. (1 - \varepsilon)^2 \mu_2}
\]  

(3)

As is seen here the dryout flux varies with the square of particle diameter in contrast with the square root dependence. In this expression there is no dependence on bed thickness as the authors studied deep beds. These authors also developed a model for channeled beds.

\[
q_d = 1.84 \left[ 1 - \frac{0.092 (1-\varepsilon) L}{\sqrt{\sigma/(\rho_1 - \rho_v) g}} \right] q_z
\]  

(4)

where \( L \) - bed thickness,
\( \sigma \) - surface tension,
\( q_z \) - Zuber's critical flux for a flat plate [4].

\[
q_z = \frac{\pi}{24} h_{lv} (\rho_v^2 (\rho_1 - \rho_v) g \sigma)^{1/4}
\]  

(5)

Hardee et al. [5] developed a model based on conservation laws for mass, momentum, and energy. The critical dryout flux was obtained by maximizing the heat flux as a function of liquid fraction. In addition, they took into account the effect of coolant subcooling with the use of the amount of energy required to heat the subcooled liquid to the boiling point.

\[
q_d = \frac{\rho_1 g d^2 \varepsilon^3 h_{lv} (1 + C_{p,i} \Delta T/h_{lv}) }{180. (1 - \varepsilon)^2 (\sqrt{\mu_v/\rho_v} + \sqrt{\mu_2/\rho_1})^2}
\]  

(6)

where \( \Delta T \) - liquid subcooling,
\( C_{p,i} \) - liquid specific heat.

This expression is similar to (3), however, it predicts a stronger pressure dependence due to the dominant role of vapor density in the equation.

Rivard [6, 7] noted that equation (6) was not appropriate with sodium, and proposed to determine the length of the boiling zone from the heat transfer in the subcooled zone by the heat conduction. He proposed to calculate the heat transfer from the bed top being at a temperature close to saturation to the bulk sodium by the following equation:

\[
T_e - T_p = \left[ \frac{q}{2400} \right]^{0.758}.
\]  

(7)
Shires et al. [8] extended the model [5] to include the effect of capillary forces: where 
\[ C \] is an empirical constant and \( C = 0.211 \) fits the experimental data [9]. The addition of 
capillary forces is an extremely important step; for most cases under fast reactor conditions, 
capillary forces are two to ten times greater than gravity and thus increase the 
\[
q_d = \frac{C \rho_j g d^2 e^3 \rho_l h_{l\nu}}{180 (1 - e)^2 \mu_l} \left[ 1 + \frac{4.29 \alpha (1 - e)}{e^3 \rho_l g L} \right],
\]
where \( C \) is an empirical constant and \( C = 0.211 \) fits the experimental data [9]. The addition 
of capillary forces is an extremely important step; for most cases under fast reactor 
conditions, capillary forces are two to ten times greater than gravity and thus increase the 
dryout flux by 2 to 11 times.

Most completely all the aspects of the problem have been considered by Lipinski 
[10-15]. His models are based on the analysis of conservation laws for mass, momentum, and 
energy with the following involved relations. The energy and mass conservation equations 
\[
\frac{d}{dz} (\rho_v v_{lv} h_{lv}) = q_v 
\]
are:
\[
\frac{d}{dz} (\rho_v v_v + \rho_l v_l) = 0 ,
\]
where \( v_l \), and \( v_v \) are liquid and vapor velocities.

The equations of motion for liquid and vapor are presented separately:
\[
\frac{1.75(1 - e) \rho_v v_{lv}}{d^2 e^3(1 - s)^2} + \frac{150(1 - e)^2 \mu_v v_v}{d^2 e^3(1 - s)^3} + \frac{dp_v}{dz} + \rho_v g = 0 ,
\]
\[
\frac{1.75(1 - e) \rho_l v_{lv}}{d^3 e^3 s^3} + \frac{150(1 - e)^2 \mu_l v_l}{d^3 e^3 s^3} + \frac{dp_l}{dz} + \rho_l g = 0 ,
\]
where \( s \) represents the effective saturation.

The first two terms in these equations are turbulent and laminar components of the 
hydraulic resistance. The pressure in the liquid \( p_l \) differs from the pressure in the vapor \( p_v \) 
due to capillary force.
\[
p_l - p_v = \frac{\sqrt{750} \alpha (1 - e) \cos \theta \mathcal{J}}{e^3 \rho_l g L}.
\]
Here \( \mathcal{J} \) represents the Leverett function of semiempirical character.
\[
\mathcal{J} = \frac{(s^{-1} - 1)_{0.175}}{\sqrt{s}}.
\]

From the above equations one can obtain the following equation for calculating the 
bed characteristics [13, 14]:

136
\[-\sqrt{150} \cos \theta \frac{\partial}{\partial z} (1 - \varepsilon) \frac{dJ}{ds} \frac{ds}{dz} \sqrt{150} \cos \theta \sigma \frac{d}{dz} \left[ \frac{1}{\varepsilon} \right] +
\]
\[+ (p_1 - p_v) g = \frac{1.75 (1 - \varepsilon)}{\varepsilon^3 d \varepsilon^2 \rho_v (1 - s)^5} + \frac{1}{\rho_v (1 - s)^3} \frac{1}{\rho_v (1 - s)^3} + \frac{1}{\rho_1 s^3} \] +
\[+ \frac{150 (1 - \varepsilon)^2 q_v}{\varepsilon^3 d^2 \varepsilon^2 \rho_v} \left[ \frac{1}{\rho_v (1 - s)^3} + \frac{\mu_v}{\rho_1 s^3} \right] +
\]
\[+ \frac{(1 - \varepsilon) w}{\varepsilon^3 d \rho_2} (\pm \frac{1.75 w}{s} \pm \frac{3.5 q}{h_{lv}} + \frac{150 (1 - \varepsilon)}{s^3} \mu_1) \]

The first two terms of this equation are the gradients of capillary pressure due to variations in the saturation and particle diameter. The second is operative in stratified beds and reduces the dryout flux in stratified beds. The third term represents the hydrostatic pressure gradient, the fourth and the fifth ones are the turbulent and laminar hydraulic resistances. The last term represents the effect of the forced coolant flow rate (w) through the plate. The signs (+) and (-) depend on the correlations of q and wh_{lv}, the upper sign being at q > wh_{lv}, the lower one - at q < w h_{lv}.

The equation (15) reduces to the algebraic one for a deep bed when capillary forces can be neglected (at \( \sigma = 0 \)). This equation covers all real flows allowing to pass smoothly from the laminar flow to the turbulent one. The influence of the term representing the turbulent resistance starts to count at a particle diameter more than 1 mm.

The model presented is valid for packed boiling region of the bed. Therefore, in case of channel formation it is necessary to determine the boundary condition at the top of the packed boiling zone. Without going into details the depth of the channeled zone can be presented as [13, 15]:

\[L_c = \frac{\sqrt{150} \cos \theta \sigma J}{\varepsilon d (\rho_p - \rho_v)} g, \quad (16)\]

where \( \rho_p \) represents the particle density.

An equation for determining the heat flux at the boundary of the packed boiling zone \( q_c \) has the following form [13]:

\[\frac{(1 - \varepsilon) q_c}{\varepsilon^3 d \rho_v g h_{lv}} \left[ \frac{1.75 q_c}{h_{lv} (1 - s)^5} + \frac{150 (1 - \varepsilon) \mu_v}{d (1 - s)^3} \right] = \]
\[= \rho_p (1 - \varepsilon) + \rho_1 \varepsilon, \quad (17)\]

where \( q_c \) represents the heat flux at the base of the channeled region.

From expressions (16) and (17) one can determine \( L_c \) and \( s \) and use as a boundary condition for the equation (15). This model does not apply if the channel length exceeds half the bed thickness.

The model considered does not provide an explicit expression for the dryout flux and is rather complicated for use. It can be simplified by reducing to the solution of the algebraic equations without allowing for special errors for moderate and deep beds. As is shown in
the maximization of the heat flux for the saturation gives the following expressions for the critical fluxes:

\[ q_d = \left[ \frac{q_e^2}{4q_i^2} + q_e^2 \right]^{1/2} - \frac{q_e}{2q_i}, \]  

where

\[ q_i = \frac{(\rho_1 - \rho_v) g d^2 e^3 H_{1v} [1 + \frac{\lambda_c}{L}]}{150 (1 - e)^2 (\nu_1^{3/4} + \nu_2^{1/4})^4}, \]

\[ q_e = \frac{H_{1v} [\rho_1 \rho_v (\rho_1 - \rho_v) g d e^3 [1 + \frac{\lambda_c}{L}]}{1.75 (1 - e) (\rho_v^{1/6} + \rho_1^{1/6})^6} \]^{1/2},

where \( \lambda_c \) is the capillary head expressed as a height of the coolant column which can be drawn into the boiling zone.

\[ \lambda_c = \frac{6 \sigma \cos \theta}{d^2 e (\rho_1 - \rho_v) g}. \]

For the laminar flow (small particles) the equation (18) reduces to (19) and for the turbulent flow to (20).

It should be noted that in a particular case when \( \lambda_c = 0, w = 0 \), the equations (19) and (20) are exact solutions for the equation (15). For shallow and moderate beds the difference between the critical flux determined from (18) and solution for (15) does not exceed 30\% and for deep beds this difference is even smaller. In case of channel formation the critical flux is determined from rather a natural assumption

\[ \frac{q_d}{q_d^*} = \frac{L}{L - \lambda_c}. \]

where \( q_d^* \) represents the critical flux obtained from (18).

Jones et al. [16] developed a model to include the shear between the liquid and the vapour, but did not include the effects of turbulent flow or capillary force. The model was specified for bottom-heated (rather than volume-heated) beds:

\[ q_d = \frac{(\rho_1 - \rho_v) g d^2 e^3 H_{1v} [ \frac{\mu_v}{\rho_v s^2 (1 - s)} + \frac{\mu_j}{\rho_j (1 - s)^3} ]^{-1}, \]

where \( s \) is determined by:

\[ 2 - 7s + 8s^2 - 3 [1 + \frac{\mu_2 \rho_y}{\mu_1 \rho_2}] s^3 = 0. \]

The equation (23) is similar to that by Hardee et al. [5] except for the different dependence on the ratio of kinematic viscosities.
Gabor et al. [17] produced a model based on bubble release from the narrowest points between adjacent particles. They derived an expression common for volume-heated and bottom-heated beds:

\[ (\rho_1 - \rho_v) g = A_1 \frac{180 \mu_v (1 - \varepsilon)^2}{d^2 \varepsilon^3} \left[ \frac{C q_{d1}}{\rho_v \Delta H_v} \right] + A_2 \frac{1.75 \rho_v (1 - \varepsilon)}{d^2 \varepsilon^3} \left[ \frac{C q_{d2}}{\rho_v \Delta H_v} \right]^2, \]

where

\[ C = 0.0157 \left[ \frac{\rho_1 - \rho_v}{d \varepsilon} \right]^{1/3}. \]

This quadratic equation is solved for \( q_c \). and \( C \) represents an empirical constant fit to their experimental data and has units of \((m^{3/3} s^{2/3})\). \( A_1 \) and \( A_2 \) are not empirical constants. They are both 1 for bottom-heated beds, and are \( \frac{1}{2} \) and \( \frac{1}{3} \), respectively, for volume-heated beds. The model successfully predicts the factor of two differences observed between the dryout fluxes for volume-heated and bottom-heated beds.

There are a number of other models [18, 19] which are less applicable to fast reactor conditions.

In [15] a comparison of calculated dryout flux data with the experimental ones was carried out. The discrepancy between calculation and experiment was determined by

\[ f = \frac{1}{N} \sum_i f_i/N \]

where \( N \) represents the number of experimental points.

\[ f_i = \frac{q_{\text{exp}}}{q_{\text{cal}}} - 1, \quad \text{if } q_{\text{exp}} > q_{\text{cal}} \] (26)

\[ f_i = \frac{q_{\text{cal}}}{q_{\text{exp}}} - 1, \quad \text{if } q_{\text{exp}} > q_{\text{cal}} \] (27)

Such determination of an error provides equal contribution of both overestimated and underestimated data (Table 1.2.3.1). presents errors for various models.

### TABLE 1.2.3.1. ERRORS OF VARIOUS CALCULATIONAL MODELS

<table>
<thead>
<tr>
<th>Model</th>
<th>Error</th>
</tr>
</thead>
<tbody>
<tr>
<td>Sowa et al. [1]</td>
<td>0.61</td>
</tr>
<tr>
<td>Dhir - Catton [3]</td>
<td>3.94</td>
</tr>
<tr>
<td>Shires - Stevens [8]</td>
<td>3.41</td>
</tr>
<tr>
<td>Lipinski [10]</td>
<td>0.54</td>
</tr>
<tr>
<td>Jones et al. [16]</td>
<td>4.80</td>
</tr>
<tr>
<td>Gabor et al. [17]</td>
<td>1.59</td>
</tr>
<tr>
<td>Lipinski [11, 13, 15]</td>
<td>0.60</td>
</tr>
</tbody>
</table>
As is seen from this Table, all the experiments are most suited by the models of Sowa et al. and Lipinski [10, 11, 13, 15]. However, each of them for specific bed characteristics can adequately enough predict the dryout flux value. To illustrate the influence of some bed parameters on the dryout flux value Figs. 1.2.3.2,3 present results of calculation by the Lipinski model. In Fig. 1.1.3.2 the dependence of the critical flux on particle diameter for various bed thicknesses. As a whole, the dryout flux increases with particle diameter and decreases with increasing bed thickness. Most strongly this dependence shows up for small-diameter particles in deep beds, but it is less strong at large diameters (due to turbulence) or shallow beds (due to capillary force). The dryout flux practically does not depend on bed thickness if it is very deep or particle diameter is also large (diameter of several millimeters). The concept of a shallow or a deep bed depends on particle size and is reflected by the size of the channeled zone and the capillary head. In Fig. 1.2.3.3 the dependence of the dryout flux on the bed thickness for various particle diameters is given. The dryout flux reaches an asymptotic value when the bed thickness reaches a maximum depth. Non-deep beds can be divided into moderate and shallow ones. The regime characteristic of a shallow bed takes place when the size of the channeled zone is insignificant as compared to bed thickness. However, the dryout flux can still depend on the bed thickness even when channeling can be neglected. The regime characteristic for the moderate bed appears when the bed thickness is much less than the capillary head (see
Fig. 1.2.3.3. Dryout heat flux versus bed thickness for LMFBR debris in nonsubcooled sodium

The characteristic size of zones for all three regimes can be expressed by the following inequality [13]:

$$L_{\text{shallow}} < 6L_c < L_{\text{moderate}} < 3\lambda_c < L_{\text{deep}}.$$  \hspace{1cm} (28)

These regimes are indicated in Fig. 1.2.3.3.

The value of the critical flux is also affected by other parameters. E.g., an increase of bed porosity results in an increase of the critical flux; the influence of the pool pressure is not simple but in a pressure range characteristic for fast reactors its increase leads also to an increase of the dryout flux.

The dryout flux is predicted to be lower with a stratified bed (in which the smallest particles are at the top) than with the same bed uniformly mixed. The reason for this is that
the top layer (where the liquid and vapour flows are largest) contains the smallest particles, and capillary force tends to draw the liquid to the region with the smallest particles. For deep beds when capillary force can be neglected the model of Lipinski predicts the dryout flux as for particles with the smallest diameters.

1.2.3.2. Mathematical methods for temperature profile calculation

In a conservative statement, one can, in principle, restrict oneself to calculations of the dryout heat flux for specific bed characteristics and heat transfer conditions and consider dryout as the limiting case for bed coolability. However, as was already noted above (see Chapter 1.1.5), in the DC-series experiments the possibility of heat removal from the dry bed was shown.

In [20, 23] there was considered one-dimensional bed high \( h \), containing uniformly distributed fuel and steel particles with the average diameter \( d \) lying on the retention plate of height \( h \) (Fig. 1.2.3.4). It is assumed that by its neutronics and physical characteristics this bed forms a subcritical configuration and the rate of heat generation in it corresponds to the decay power. Heat is removed from the bed by heat transfer to the coolant pool both above the bed and under the plate bottom. By its temperature and aggregate state the bed can be at different stages (Fig. 1.2.3.4). During stage A the whole bed contains liquid coolant and solid particles. During stage B inside the bed a boiling zone is formed with liquid sodium above and under it. During stage C the heat flux exceeds the dryout flux and in addition to the zones corresponding to the stage B a dry zone is formed with the fuel in this zone not melted down. During stage E the dryout zone extends up to the support plate and depending on conditions of heat removal to coolant under the plate, its melting can occur. During stage I the molten fuel can lie on the plate and again, depending on heat removal conditions, melting of the plate is possible. From the onset of fuel melting the bed height decrease due to fuel compaction in the melting zone. The same can be said when steel particles present in the bed are melting.

The problem of the temperature field analysis in the bed of particles debris is reduced to solving the heat conduction equations for different zones (except for the boiling zone) with appropriate boundary conditions. For this purpose it is essential to determine the effective coefficients of thermal conductivity for each zone. The thermal conductivity of a system consisting of a liquid and solid elements can be obtained in the general form by solving the problem of thermal conductivity for the unit cell in two- and three-dimensional geometry as was done, e.g., in [25]. For a system with spherical solid particles the most commonly used expression obtained by Kamphf and Karsten [26] is:

\[
\lambda = \lambda_{\text{nu}} \left[ 1 - e^{2/3} \left[ 1 - \frac{1}{1 + e^{2/3} \left( \frac{\lambda_{\text{nu}}}{\lambda_{s}} - 1 \right)} \right] \right]. \tag{29}
\]

At the development of natural circulation in the bed, when the Rayleigh number exceeds the critical value, it should be used the effective coefficient of thermal conductivity in the form:

\[
\chi = \lambda \cdot \text{Nu} \tag{30}
\]
Coolant $\theta_2, \alpha_2$

![Diagram of particle bed stages](image)

Coolant $\theta_1, \alpha_1$

A B C D E I

Fig. 1.2.3.4. Characteristic particle bed stages
According to experimental data obtained from the D-series experiments the dependence of Nu on Ra has the following form:

\[ \text{Nu} = \left[ \frac{\text{Ra}}{0.76} \right]^{0.34}. \]  
(31)

The critical Rayleigh number determined at Nu = 1 as is seen from (31) is equal to 0.76. The Rayleigh number for porous medium with heat release has the following form:

\[ \text{Ra} = \frac{\lambda_{\text{Na}} \rho_{\text{Na}} \beta_{\text{Na}} \kappa Q_v h^3}{2 \lambda^2 \alpha_{\text{Na}} \mu_{\text{Na}}}, \]  
(32)

where \( \lambda_{\text{Na}}, \rho_{\text{Na}}, \beta_{\text{Na}}, \alpha_{\text{Na}}, \mu_{\text{Na}} \) are thermal conductivity, density, thermal expansion coefficient and temperature diffusivity dynamic viscosity of sodium, respectively \( K \) represents the permeability, \( \lambda \) - thermal conductivity of the bed without convection.

In the dryout bed, in addition to thermal conductivity, the radiation should be also taken into account. Therefore, the effective thermal conductivity has the following form:

\[ (\lambda_{\text{eff}})^{-1} = \frac{1}{T_2 - T_1} \int_{T_1}^{T_2} \frac{1}{\lambda + \Delta \lambda_r} \, dT, \]  
(33)

where \( T_1, T_2 \) are the temperatures limiting the range of its variation from the coolant boiling point to the fuel melting temperature; and \( \Delta \lambda_r \) is a radiation increment of the thermal conductivity.

According to [27] this increment can be written as:

\[ \Delta \lambda_r = 4 \, n_r^2 \, \sigma \, e \left[ \frac{1}{1 - e} - 1 \right] \, d^3 \, T^3, \]  
(34)

where \( \sigma \) is the Stephen - Boltzman constant; \( e \) - the emissivity factor; \( d \) - particle diameter; \( n_r^2 \) - refractive index; and \( T \) - the absolute temperature.

A significant contribution into the effective thermal conductivity can be made by the particle sintering effect. However, at present, there is no sufficiently validated experimental data that would allow to take into account this circumstance. Not taking into account of this effect can be classified as a calculation margin.

To calculate the heat transfer coefficient from the bed to sodium pool above it the empirical expression [11] is used.

\[ T - \theta = \frac{\theta}{2400}^{0.758}. \]  
(35)

In the models considered, at the onset of boiling the size of its zone is calculated, the conditions for channeling are verified and according to it an expression for the dryout flux is chosen. The critical size of the boiling zone is determined from the heat generation and dryout flux:

\[ h_{\text{boil}}^c = \frac{Q_d}{Q_v}. \]  
(36)
In the melt zone heat transfer is treated as conduction and natural convection in terms of an effective thermal conductivity. Fig. 1.2.3.5 [23] presents the distribution of various zones within the bed consisting of fuel particles as a function of heat generation (a bed height of 15 cm, particle diameter of 0.2 mm, subcooling sodium pool of 400°C, the support plate heat transfer coefficient of $10^3$ W/m$^2$°C).

For a more correct evaluation of the debris bed behaviour it should be considered the problem in a transient statement, as the decay heat release varies with time and the bed itself has a particular heat capacity and corresponding thermal inertia that can not be accounted for in the steady-state model described above. However, the steady-state model is more exact from the view point of taking into account various physical characteristics of the process and involves experimentally validated dependencies of the dryout fluxes.

The bed of degraded core debris is a multicomponent (fuel, steel and coolant) system with changing aggregate states and various heat transfer mechanisms (heat conduction, radiation and convection both in a single phase coolant and at boiling). An exact mathematical description of transient processes taking place in the bed is, in principle, possible, but the solution of such a problem is connected with great calculation difficulties. But the problem is substantially simplified at reducing it to solving the transient equation of heat conduction in all zones. In [21] an original approach was proposed to passing over from the convective problem solution in the sodium boiling zone to solving heat conduction problem by introducing the effective thermal conductivity. By considering the laminar flow of liquid and vapour under conditions of capillary force the following expression for the effective heat conduction was obtained:

$$\lambda_{\text{eff}} = \frac{K' h_{lv}}{v_l + v_v} \frac{\partial P_v}{\partial T}, \quad (37)$$

where $K$ represents the bed permeability, and $K_l$, $K_v$, $v_l$, $v_v$ are relative permeabilities, kinematic viscosities of liquid and vapour, respectively.

$$K = \frac{d^2 \varepsilon}{150 (1 - \varepsilon)}, \quad (38)$$

$$K_l = (1 - s)^3, \quad (39)$$

$$K_v = s^3 \quad (40)$$

In this case the effective saturation of liquid is determined from the following expression:

$$s = [1 + \frac{T - T_{\text{sat}}}{T - T_{\text{sat}} \left( \frac{dP_v}{dT} \right)^{0.175}} - 1]. \quad (41)$$

This approach was used in [21, 24] when developing multi-dimensional models for calculating transient temperature fields in the bed.
Fig. 1.2.3.5. Variation of the phase composition vs bed power density in a fuel particle bed for bottom cooling conditions
CONCLUSIONS

Analytical studies of the process of heat removal from degradated core debris have received world-wide distribution. They mainly reduce to the development of relationships for critical heat fluxes leading to dryout of the porous heat generation bed and of procedures and codes for the temperature field analysis in beds with various characteristics. Subsequent analytical studies should be directed towards including into the models:

- mixed heat transfer mechanisms,
- stratification of bed particles in size,
- updated empirical and semiempirical relationship for the critical fluxes, coefficients of thermal conductivity of the bed under various conditions, heat transfer coefficients to coolant pool around the bed, etc.,
- the mechanisms of steel particles behaviour in the bed when their melting reoccurs, and
- arbitrary geometry corresponding to possible arrangements of degradated core debris.

REFERENCES

2. SODIUM-WATER INTERACTIONS IN STEAM GENERATORS, SODIUM FIRES: TESTS AND ANALYSES

2.1. SODIUM-WATER INTERACTIONS IN STEAM GENERATORS

Because of the radioactivity of the sodium in the primary circuits and the possibility of sodium-water interactions in steam generators, LMFRs are equipped with non-radioactive intermediate (secondary) sodium circuits. The intermediate heat exchangers thus constitute an inner containment barrier. In the case of failure of a water filled pressurized steam generator tube, water is injected at high pressure into the sodium and a violent sodium-water interaction occurs. This sodium-water interaction will give rise to peak pressures that must be accommodated by straining the steam generator shell and the pipelines in the secondary circuit. Moreover, if the pressure in the system gets too high, a special rupture discs in sodium and/or gas will be destroyed, thereby relieving the pressure and ejecting sodium sodium hydroxide, sodium oxide and hydrogen into a special collector vessel. This pressure relief system is very important and protects the intermediate heat exchanger from damage. The sodium-water interaction is immediately detected by instrumentation and the plant will be shut down if a large leak occurred [51]. The said basis physical processes are discussed below.

2.1.1. Sodium-water steam generator design features

One of the principal problem in the development of the sodium - water steam generator (SG) design is the choice of the heat exchange tube bundle characteristics. Specific requirements are also placed upon sodium-water SG structural materials. The corrosion-erosion resistance of materials in sodium-water reaction products becomes of great importance. From this point of view, high-nickel alloys (30-40% Ni) have the best characteristics; austenitic chromium - nickel steels of X18H9 type being somewhat inferior to them. Perlitic steels 2½ Cr 1Mo and chromium steels with 9-12% Cr are most susceptible to corrosion-erosion failure under reaction products. It should be taken into account at the same time that perlitic steels are low-cost, relatively workable but have low corrosion resistance and are prone to pitting on the steam-water side. Chromium steels, possessing some merits of perlitic ones, have an advantage of higher corrosion resistance over the latter but in some cases may prove to be structurally unstable. Austenitic steels are highly workable, have rather high general corrosion resistance, but are susceptible to corrosion stress cracking and, under certain conditions, to intergranular corrosion. High-nickel alloys have high corrosion resistance but low thermal conductivity, are difficult from the point of workability and have also high cost. Besides, some steels have certain limitations on temperature conditions (for example, for 2½ Cr 1 Mo steel, stability of its mechanical properties is guaranteed at a temperature not higher than 520°C). It should be also noted that practically all of the above steels have adequate corrosion resistance in pure sodium and all corrosion problems take place on the steam-water side. So it is seen that the choice of materials for the heat exchange surfaces is rather a complex problem. Taking into account various properties of steels and contradictory requirements placed upon them, the SG design features, such as unit heat capacity, tube bundle characteristics, maintainability, etc., have not the least and, sometimes, even decisive role at the choice of structural materials. To date, there are examples of the realization of all the above types of steels.

Speaking about design features, it should be noted at least two specific features. The first is the small distance between tubes in the tube bundle. In the presence of microdefects
in the heat exchange tube a small water leaks into sodium occur with the formation of the chemical reaction products flame due to coolant interaction with water. The rate of corrosion-erosion damage of the adjacent tube material by reaction products formed in the flame depends, to a considerable degree, on the tube interspace. When choosing the tube interspace, the designer should be governed, on the one hand, by the limitations on the overall bundle dimensions, and, on the other hand, he should take into account the consequences of an accidental contact of coolants at small water-into-sodium leaks. The practice has shown that for the designs made up of single modules of small heat capacity, and in which the repair problems can be solved by simple replacement of one module by another, the optimum tube interspace is within 12-15 mm. For SGs of larger unit capacity along with using corrosion-resistant steels (for example, Incolloy 800), the tube interspace is sometimes increased up to 30-40 mm.

The second design feature grows from a desire to ensure the integrity of the steam generator vessel and sodium circuit under conditions of large water-into-sodium leaks (appreciable tube failures). As experimental studies have shown, an efficient means to reduce the rate of hydrodynamic effects at a contact of large amounts of sodium and water is the use of expansion gas spaces. For the designs of large unit heat capacity the expansion (damping) gas space is provided, as a rule, within the SG vessel. In designs made up of several vessels it is technologically difficult to maintain and control the sodium level in each of them. In this connection for the above SG types, a common level of coolant in a separate tank (expansion tank) is arranged.

It should be noted that the problem of sodium-circuit hydraulic resistance is of vital importance due to some peculiarities of emergency processes. Accidental pressure in the sodium volume near a large water leak into sodium depends not only on the leak rate but also on the hydraulic "compliance" of the sodium circuit. The experience indicates that, in connection with the above circumstances, the sodium circuit hydraulic resistance within the boundaries of the "SG coolant inlet expansion gas space" section should not exceed of 0.3 MPa.

The aim is to ensure the best performance of the design taking into account all possible emergency processes at contact of coolants caused by some specific features. At present, two types of design are under consideration: the integral and sectional ones. But before dealing with the specific features of these designs, they should be defined some concepts characterizing the steam generator as regards its design and operation. Depending on the function performed, some specific elements can be distinguished: evaporator, superheater and reheater (Fig. 2.1.1.).

2.1.2. Physical phenomena of sodium-water reaction

The sodium-water chemical interaction proceeds in two stages: at the first stage the reaction proceeds at a high rate with a release of gaseous hydrogen and heat:

\[ \text{Na} + \text{H}_2\text{O} = \text{NaOH} + \frac{1}{2} \text{H}_2 + 140 \text{ kJ/mole}. \]  

This reaction is practically irreversible because of a very high equilibrium pressure of hydrogen.

At the second stage the chemical interaction of products from the first stage of the reaction with excessive sodium takes place (excess sodium is characteristic of water leaks under steam generator conditions):
Fig. 2.1.1. Three types of sodium-water steam generator integral circuit (a) and section circuit (b)
1 - economizer; 2 - evaporator; 3 - superheater; 4 - cut-off valve
\[ 2\text{Na} + \text{NaOH} = \text{Na}_2\text{O} + \text{NaH}; \quad (2) \]
\[ \text{Na} + \frac{1}{2} \text{H}_2 = \text{NaH}. \quad (3) \]

Then the total reaction will have a form:
\[ 4 \text{Na} + \text{H}_2\text{O} = 2\text{NaH} + \text{Na}_2\text{O}. \quad (4) \]

Thus, the sodium-water interaction is rather a complicated, multistage process where successive reactions with sodium hydroxide and hydrogen formation followed by their interaction with sodium take place. The final concentration of water-sodium interaction products is determined by the thermodynamic equilibrium conditions and the time of reaching the equilibrium state by kinetics of the reactions proceeding.

Reference [1] presents in detail a general approach to the determination of the state of sodium-water interaction products at thermodynamic equilibrium. Data on the products state have been obtained both with the use of calculation methods and by means of analysis of samples taken from tests on the investigation of the water interaction with sodium. The method of thermodynamic calculations is based on solving a system of equations obtained with the use of the law of mass action for independent reactions proceeding in the given system and the balance equations for the components participating in the reaction. In carrying out the calculations it is usually assumed that the reaction (1) proceeds to the end, the quantity of sodium in the system exceeds the quantity of water penetrating into it, i.e., \( n^0_{\text{Na}} > > n^0_{\text{H}_2\text{O}} \) and all the interaction products are present in sodium in the dissolved state. The calculations have shown that with a temperature rise from 300 to 500°C the ratio of sodium hydroxide-to-oxide equilibrium concentrations decreases from 0.17 to 0.007, and with the growth of \( n^0_{\text{O}}/n^0_{\text{H}} \) the fraction of hydrogen in the form of hydroxide also increases. The latter should result in that the equilibrium pressure of hydrogen above oxygen- and hydrogen-bearing sodium will decrease with the growth of \( n^0_{\text{O}}/n^0_{\text{H}} \). This has been confirmed experimentally [2]. At penetration of a considerable amount of water into sodium the condition \( n^0_{\text{Na}} > > n^0_{\text{H}_2\text{O}} \) is not fulfilled. The concentration of compounds formed may exceed their solubility in sodium. Under these conditions the caustic phase is formed consisting of hydroxide and dissolved sodium oxide, hydride and sodium. The thermodynamic analysis of the caustic phase composition under the assumption that solubility of sodium in hydroxide is small as compared with that of hydride and oxide has shown that the main reaction products in the presence of liquid sodium as an independent phase would be sodium hydroxide, hydride and oxide [3,4]. Their molar fractions at 450°C and \( n^0_{\text{O}}/n^0_{\text{H}} \approx 0.5 \) are equal to 0.46, 0.51 and 0.03, respectively. The hydroxide fraction increases with temperature and \( n^0_{\text{O}}/n^0_{\text{H}} \). In this case it is particularly important to know that the melting point of the caustic phase will exceed the melting point of pure hydroxide. Under real conditions of the caustic phase formation the melting point does not usually exceed 400°C. At present, general rules describing the equilibrium pressure of hydrogen in the gaseous phase under the sodium-oxygen-hydrogen system have not been developed. For a wide range of concentrations the dependence of the hydrogen partial pressure on temperature according to data of [5] is described by the following equation (coefficients 'a' and 'b' - see Table 2.1.1.):

\[ \lg P_{\text{H}_2} = b - a/T. \quad (5) \]
TABLE 2.1.1. VALUES OF A AND B COEFFICIENTS FOR VARIOUS SODIUM HYDRIDE CONCENTRATIONS IN SODIUM

<table>
<thead>
<tr>
<th>Coefficient values</th>
<th>NaH fraction, %</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>10</td>
</tr>
<tr>
<td>a</td>
<td>6222</td>
</tr>
<tr>
<td>b</td>
<td>10.70</td>
</tr>
</tbody>
</table>

Solubility in sodium (mln⁻¹) of oxygen, hydrogen, and of sodium-water interaction products are described by the following equations [1]:

\[
\lg C_\text{O} = 6.257 - 2444/T, \tag{6}
\]

\[
\lg C_\text{H} = 6.067 - 2880/T, \tag{7}
\]

\[
\lg Q_{\text{O(H}_2\text{O)}} = 6.833 - 2660/T. \tag{8}
\]

In (8), solubility of sodium-water interaction products has been conventionally calculated for oxygen contained in water.

In Fig. 2.1.2 a diagram of equilibrium states of the sodium-water system as a function of temperature and molar fraction of water in reactants composition, is presented [5]. As is seen from the diagram, the final products of the sodium-water reaction can be sodium hydride and oxide in sodium- and sodium hydroxide-based liquid solutions, hydride and hydroxide solid solution, as well as aqueous solution of alkali and gaseous hydrogen.

A system of equations describing the variation of the sodium-water interaction products concentration in sodium taking into account kinetics of chemical reactions has been considered in detail in [1]. Into the above equations enter coefficients depending on the rate of hydrogen and sodium hydroxide interaction with sodium, as well as on the rates of reverse reactions. At present, the rates of direct reactions are known. So, according to the data of [2, 6], the homogeneous and heterogeneous interaction of hydroxide with sodium is described by equations:

\[
\lg K_{\text{Hom.}} = (12.45 - 6770/T), \tag{9}
\]

\[
\lg K_{\text{Het}} = (5.9 + 4060/T). \tag{10}
\]

The question of the type of sodium hydroxide interaction with sodium has not been completely elucidated. It can be considered that in the case of a crystalline phase of hydroxide, the interaction will be of a heterogeneous character and with hydroxide dissolved in sodium the interaction will be homogeneous. With a jet leak of water into sodium at small leaks, a mixed character of interaction is possible.

An important parameter is the fraction of gaseous hydrogen release into the steam generator gas space. It depends on the conditions for formation of hydrogen bubbles in sodium and their interaction with coolant.
Assuming that a spherical bubble in moving sodium interacts with sodium kinetically, the pressure and temperature of hydrogen in the bubble being equal to the corresponding parameters of sodium, in reference [1] an equation has been proposed for the calculation of the bubble diameter variation from its initial dimension \(d_b\) at its displacement, \(X\):

\[
\frac{d|d_b|}{dx} = 2Tf(x) W_b T + \left( \frac{d_b}{2T} \right) \frac{dT}{dx} - \left( \frac{d_b}{3P} \right) \frac{dP}{dx} - \quad (11)
\]

where \(x\) represents the distance from the leak site,

\(T\) and \(P\) - temperature and pressure at point \(x\),

\(f\) is determined from the relation:

\[
\lg f = (6.63 - 3400/T) \text{ cm s}^{-1} \quad (12)
\]

Where \(W_b\) represents the velocity of bubble motion in the circuit.

A disadvantage of equation (11) for practical use is that it is necessary to know (or specify) the initial diameter of bubble.

In [7] an original relation was obtained for calculating the variation of the gaseous hydrogen volume flow rate in sodium for a bubble moving from the place of leakage through the circuit (at a specified velocity):

\[
\frac{dG}{dt} = -K_p G^{2/3} + \frac{G}{T} \frac{dT}{dt} - \left( \frac{G}{P} \right) \frac{dP}{dt} \quad (13)
\]
where the constant $K_p$ is determined from the relation 14:

$$\lg\left(\frac{1}{3}K_p\right) = 6.5 - \frac{5318}{T}.$$  (14)

The relation (13) is very convenient to use for practical purposes, as at known values of a water-into-sodium leak and of sodium pressure and temperature in the leak region, one can always determine the initial volume flow rate of hydrogen.

2.1.3. Sodium-water reaction within free volumes

Sodium-water chemical interaction may be accompanied by an increase of pressure and temperature. To illustrate this point, as an example, in Fig. 2.1.3 are presented the results of a calculation of these parameters for a sodium interaction with various amounts of water [8]. It has been assumed in the analysis that the reaction goes to completion within a rigid container with specified conditions of its interaction with the external medium; hydrogen produced occupies the volume formed as a result of the difference between the volume of reactants and that of the final products. It has been noted that the process of sodium - water interaction at one reactant leaking into another in a free volume is accompanied by pressure pulses in the reaction zone [8].

In reference [9] the nature of such a phenomenon was studied. It was also assumed that the cause of a stepwise pressure rise might be the hydrodynamic effects of a pressurized water jet flow; the effects of fast water evaporation at its penetration from a high-pressure region into the low-pressure and high-temperature sodium; a non-uniform character of the reaction itself because of a change in the reactants’ contact area. By excluding simultaneous

\[ p, \text{ MPa} \quad T, \text{ °C} \]

\[ 0 \quad 1000 \quad 1500 \]
\[ 10 \quad 10^2 \quad 10^3 \]
\[ 0 \quad 500 \]
\[ 500 \quad 1000 \quad 1500 \]

Fig. 2.1.3. Pressure and temperature change at the interaction of 1 mole of sodium with various amounts of water.

1 - isothermal reaction pressure;
2 - pressure values corresponding to ideal gas low assumption;
3 - adiabatic process temperature
action of the above factors (water injection into cold water, into gas and organic fluid at a high temperature and into molten sodium) it was experimentally demonstrated that a stepwise pressure rise was caused just by the reaction of interaction.

Hydrogen being one of the reaction products, King [8] made an assumption on its influence upon reactants mixing. By varying the initial pressure of reactants within a range of 0-1.5 MPa (thus changing the volume of the gaseous phase), King noted the difference in the reaction rates. King also studied the effect of the initial temperature on the character of the reaction. For mixing of sodium-potassium alloy with water the reaction proceeded so vigorously that damage to a tank, previously tested for a pressure of 20 MPa, was observed. At preheating of sodium-potassium alloy up to 320°C, its reaction with water proceeded without any abrupt pressure changes. Studies of interaction between alkaline metals (including sodium) and steam have revealed a more smooth and safe character of the reaction as compared with their interaction with water. During injection of liquid sodium into water at room temperature there have been noticed more vigorous dynamic effects compared with those observed during water injection into liquid sodium [9]. This phenomenon can be explained using King’s data on the effect of the initial temperature of the medium in which the interaction takes place. At the sodium-into-water injection the temperature of the excessive medium was only 20-40°C compared with 170-240°C at the water-into-sodium injection. In the first case the phase transformation of sodium hydroxide occurred later, that increased the reaction nonuniformity.

2.1.4. Small water-into-sodium leaks in steam generator

The leaks and their classification. Water-into-sodium flow is connected with the appearance of new failures or with opening of those already present in structural materials separating water and sodium. As has been shown by sodium-water SG operating experience, in the initial period of operation the development of those defects is most probable. Non-detection has been caused by insufficiency of inspection devices used during SG fabrication. As a rule, the size of these defects is very small. For long-term SG operation the appearance of defects caused by corrosion processes (especially on the water side) or as a result of vibration wastage of tubes in spacing grids or in other structural elements is possible. The size of these defects at the beginning of a water-into-sodium leak through them is also insignificant but it can exceed by some orders of magnitude the defects missed in the process of fabrication. At the last stage of SG service life the probability of the leak appearance increases and it should be expected that both, the initial defect and, therefore, the initial flow rate of water leaking into sodium would increase. Thus, the most probable way of water-into-sodium penetration is through defects, the size of which is some fraction of a millimeter, only in extreme cases reaching an equivalent size of 1mm. The initial water-into-sodium leakage value, as a rule, will be tenths or even hundredths of a gram per second and only in some rare exceptional cases may reach one gram per second or a higher value. One of the main peculiarities of a water-into-sodium leak is that in this case self-development of a defect occurs that results in a subsequent increase of water flow rate and in a failure of tubes adjacent to the failed one. A typical variation of water-into-sodium leak rate with time is presented in Fig. 2.1.4. The total time of leakage can be divided into four characteristic periods: (1) the time $\tau_1$ - leak is maintained at about some average level, some reduction and even the cessation of leak for a prolonged time is possible followed by its spontaneous resumption; (2) the time $\tau_2$ - the final period of leak self-development when during a short period of time the water flow rate increases (by an order of magnitude or more) in a few seconds. As a rule, in this stage a strong effect of the jet upon the tubes adjacent to the failed one begins; (3) the time $\tau_3$ - through a defect formed as a result of leak self-development an
Fig. 2.1.4. Characteristic water into sodium leak rate as function of time (a) and material damage mode during self-developing leak process caused by tube failure (b) or by tube to tube plate joint failure: 1 - reagents interaction area; 2 - material damage area; 3 - tube wall; 4 - initial outflow channel; 5 - material destruction front movement direction; 6 - tube plate

outflow of water at a large, practically constant flow rate takes place; at the end a through defect is formed at the tube neighboring the failed one; (4) the time $\tau$ - an increase of flow caused by an appearance and growth of the secondary defects in heat exchange tubes (a corrosion-erosion failure or a failure at metal overheating). It is obvious that at a sudden opening of a significant defect the initial sections of the curve presented in Fig. 2.1.4 may be lacking. The question of leak classification is of an arbitrary character determined by some adopted criteria that characterize the disturbance of hydrodynamic stability of the sodium circuit due to the action of reaction products upon structural materials, and to a considerable degree is determined by steam generator and sodium circuit design.

Depending on the character and consequences of the effect of water-into-sodium penetration upon the thermohydraulic parameters of the sodium circuit and steam generator structural elements, two principal types of leaks are distinguished.

A large leak is characterized by short but considerable flow rates of water into sodium and is accompanied by a considerable change in hydrodynamic (pressure, flow rate of coolant) and temperature characteristics of the sodium pipeline, by a high rate of corrosion of structural materials in the water leak area.

A small leak is characterized by an increase of concentration of impurities (products of sodium-water interaction reaction) in coolant, by the presence of corrosion-erosion material failure in the leak area. In this case, as a rule, no departure of hydrodynamic parameters of the sodium circuit from the normal ones is observed. A water-into-sodium leak with the same flow rate of water may be classified as a "large" leak for a single-tube sodium channel and as a "small" one for a heat exchanger with a large sodium volume.
Sometimes for the sodium-water SG the so-called intermediate leaks are distinguished by water-into-sodium flow rates of several grams or even of several tens of grams per second and by the fact that, under conditions of large sodium circuits, they do not cause any marked hydrodynamic effects (characteristic of the large leaks); at the same time the intensity of corrosion-erosion processes of material wastage having passed through a maximum has a tendency to decrease. In some cases, when the leak does not strongly affect structural material of adjacent tubes, and the main corrosion process takes place at the site of the initial defect, it is characterized as a "microleak". A characteristic diagram of a possible scenario of evolution of one leak into another is presented in Fig. 2.1.5 [10]. Small leaks are the most probable ones for steam generator operation. A large leak, as a rule, is the consequence of subsequent failures of the heat exchange surface in the area of a small leak, and very seldom appears as an initial leak (for example, because of vibration or corrosion tube wastage around the periphery).

Main characteristics of small water-into-sodium leaks. A small-rate water-into-sodium leak, its accompanying processes and their effects are the most important for SG performance and safety due to the relatively high probability of occurrence of this defect. Furthermore,
a small leak is characterized by some peculiarities that put specific requirements on the steam generator safety system in an emergency situation and on the coolant purification systems in the post-accident period. Firstly, a tendency to self-development of an originally small leak puts rigid requirements on leak indication and localization devices in relation to their speed of response and sensitivity. Secondly, the formation of a high-temperature reaction flame in the leak area leads to an intensive corrosion-erosion failure of tube bundle material. In some cases the latter may be the cause of the development of a small leak into a large one followed by penetration of considerable amount of water into coolant that calls for considerable impurity capacities of the purification systems and for a high purification rate.

The leak self-development processes. It has been experimentally revealed that small leaks with water-into-sodium flow rates up to 1 g/s are prone to self-development with time. For example, a leak with a water flow rate of 0.02 g/s appearing in a tube of 2¼ Cr 1 Mo steel with wall 2.5 mm thick remains at a constant level for 10 min, and then sharply increases up to several grams per second. The character of a failure of wall material in the process of the water-into-sodium leak self-development is schematically shown in Fig. 2.1.4,b. A gradual failure of steel on the side of the water-into-sodium leak issue takes place with the failure front moving deep into the heat transfer wall. As a result of the failure zone propagation the diameter of the channel increases but the amount of water leaking out is controlled by the size of the original defect which little changes on the water side. It continues in such a manner until the opening of the remaining strap diaphragm would start to increase as a result of corrosion-erosion effects. In this case (Fig. 2.1.4,a) a sharp increase of water flow rate occurs. It has been noted [11] that in the case of leakage through a defect at a tube-to-tubeplate attachment, simultaneously with a steel failure along the generatrix of the leak channel an intensive corrosion failure of the tube wall take place in the region of the direct contact of water leaking out with excessive sodium (in the plane of the tubeplate passed over by coolant). At such a development of the process a through damage of the tube wall in the above zone (and its related sharp increase of flow rate), as a rule, occurs prior to penetration of the cavity to the whole length of rather a long channel through the tubeplate thickness (Fig. 2.1.4,b). It has been experimentally determined that with increasing sodium temperature the rate of failure front propagation along the channel increases. With the growth of the original defect, i.e., with an increase of the initial leak, the rate of self-development also increases, but beginning with a specific value of its flow rate equal to about 1.5 g/s the rate of self-development decreases. The mechanism of leak self-development is usually related to high temperatures in the reaction zone (up to 1200-1400°C) and to corrosion-active products. So, in [12] self-development is explained by the "tunnel burner" effect. However, for such a mechanism it is difficult to understand a strong dependence of the self-development process on sodium temperature: a temperature rise from 300 to 450°C leads to an increase of the structural material failure rate by an order of magnitude. Special experiments have shown [13] that a leak of water with mass content of about 5% sodium hydroxide gives the same rates of steel damage as a water leak into sodium. This points to the determining effect of the corrosion failure on the process of leak self-development. The presence of alkaline solution in the channel at a water-into-sodium leak has been experimentally demonstrated.

All the materials studied: perlitic and austenitic steels (2¼ Cr1Mo and X18H10T), high-nickel alloys (30-40% Ni) are characterized by the principal material failure on the side of the water-sodium interaction zone with its gradual propagation deep into the leakout channel. However, in this case, the size of the defect formed as a result of material removal from the leakout channel decreases in the "perlitic, austenitic steels, high-nickel alloys" row: for the latter it is, as a rule, less than one millimeter, and for perlitic steel it can reach 2 mm
and more, with corrosion failures over an area up to 1 cm² observed at the surface of steel being in contact with sodium, near the issue of the water-into-sodium leak. Such failures propagated to a small depth, fractions of a millimeter. A discrepancy in the defect size is mainly explained by the difference in specific rates of integral material removal that can reach three orders of magnitude. The above pattern of the original defect development ensures retaining of water flow rate at a constant level for a definite time. It has been noted that at the first stage of leak self-development (up to its sharp increase), under conditions of the same initial mass flow rates, a steam escape is accompanied by about two times more intensive removal of material from the channel as compared with that at a water leak. A sharp increase of water-into-sodium flow rate at a certain stage of leak self-development is connected with an increased risk of corrosion-erosion damage to steam generator elements that are in the leak zone. In this connection, as regards the correct development of requirements on the steam generator protective systems and its safety assurance, it is very important to know the relationship between the original leak size and the time interval during which this leak remains unchanged. For example, Fig. 2.1.6 presents the results of experimental studies of this dependence for 2¼ Cr 1 Mo and austenitic steels [14, 15]. As is shown by experiments, an increase of the initial flow rate from 0.001 to 0.1 g/s results in a reduction of the constant leak time almost by an order of magnitude. And in this case the above noted "incubation" period of leak self-development for austenitic steel is about 3-5 times more than a similar period for 2¼ Cr 1 Mo steel. It has been also found that sodium temperature substantially affects the time of a constant leak. Therefore, a temperature increase from 360°C to 530°C decreases this parameter nearly by an order of magnitude [16].

The processes of corrosion-erosion failure of steels in a small - rate water-into-sodium leak zone. Beginning with some definite size of a defect in the steam generator heat-exchange surface a drop-like leak changes to a jet one. In this case the sodium-water interaction reaction flame is formed which at a certain ratio between the defect size and the tube interspace geometrical characteristics can reach the adjacent tube wall. It has been experimentally found that the effect of the reaction flame upon wall material results in a failure of the latter, the failure rate depending on water (steam) flow rate, their temperature, sodium temperature, its velocity, defect shape and tube interspace geometry.

In Fig. 2.1.7 is presented a diagram of the reactants interaction flame that was obtained on the basis of experimental results at water injection into sodium parallel to the metal plate surface [12]. It has been found that immediately at the leak site there is a high-temperature zone of chemical reaction, the completion of the reactants interaction process taking place in the reaction "cone" located after the high-temperature zone. It was experimentally recorded that a maximum reaction flame temperature reached 1300-1400°C and the temperature of the wall upon which the flame was acting was 1000-1200°C. The geometrical characteristics of the flame are of practical interest as regards estimating the consequences of reaction products upon tube bundle materials. In [12] for the water-into-sodium leak, the following relationship between the defect diameter \(d_0\) and the length of the reaction flame \(L\) was obtained:

\[
L/d_0 = 280. \tag{15}
\]

For the steam leak this relationship somewhat differs [17]:

\[
L/d_0 = 160, \tag{16}
\]
Fig. 2.1.6. Time period from leak beginning until sudden increase of water leak rate (sodium temperature 450°C; wall thickness 2.5): 1 - 2 1/4 CrMo steel; 2 - X18H9 steel

Fig. 2.1.7. Reaction tongue structure picture obtained by imprints on parallel oriented metal plate:
1 - chemical reaction high temperature zone;
2 - reaction "cone";
3 - high temperature reaction products zone
the maximum flame diameter \[17\] being reached at a distance of \(12d_0\) from the site of issue. In \[18\], based on processing of results from investigations by many authors, for the steam-sodium system there was proposed a dependence of the flame length on the opening diameter and steam pressure \(\Delta p\) in the following form:

\[
L = 9.3d_0/\Delta p,
\]

(17)

where \(d_0\) represents the opening diameter, mm; and \(\Delta p\) the steam pressure, kg(f)/cm².

As was noted above, at the first, most rapid stage of the water-sodium chemical reaction, gaseous hydrogen and sodium hydroxide are generated with heat release. Corrosion-activereaction products heated up to a high temperature are moving with a high velocity along the flame axis towards the adjacent heat-transfer tube. In this case the mechanism of the tube wall material failure may be presented as follows: (a) tube wall heating by the reaction flame, (b) corrosion of wall material under the action of sodium hydroxide, oxide and hydride, and (c) erosion removal of corrosion products from the area of the reaction flame effect by jet. In the process, clean (not exposed to the corrosion effect) metal layers are uncovered which, in their turn, start being exposed intensively to corrosion wastage under the action of reaction products.

Thus, it can be stated that the failure of steel in the sodium-water reaction products exposure region is of the corrosion-erosion character.

It was found by experiments \[17\] that the most intensive failure of material takes place in the flame region with parameters \(L/d_0 \leq 40\) and a maximum at \(L/d_0 = 25\). In Fig. 2.1.8 is presented the typical variation of the tube wall thinning rate under the effect of reaction products for various flame zones and various types of steels (perlitic \(2\frac{1}{4}Cr\ 1Mo\), chromium \(12Cr\) and austenitic) at a water-into-sodium leak through an opening with a characteristic size of 0.5 mm (the intermediate leak region) Similar results on the investigation of the dependence of the \(2\frac{1}{4}Cr\ 1Mo\) material failure rate on the parameter \(L/d\) have been obtained \[19\]. As regards the influence of various thermohydraulic and design steam generator parameters on the rate of the corrosion-erosion failure of steel in the small leak zone, the following can be noted. A steam-into-sodium leak provides a more concentrated center of damage as compared with the water leak. For steam leaks, the rate of steel failure also increases. In \[12\] it has been noted that a steam leak at a temperature of 340°C is accompanied by a steel failure rate 6.5 times more than at a water leak at the same temperature and with the same mass flow rate.

At present there is no consensus on the extent of the influence of the heat-transfer surface cooling process upon the rate of material failure within the reaction zone, because most experimental studies have been performed at the isothermal facilities. Therefore, in the experiments on the investigation of this effect no marked difference in the failure rate of cooled and uncooled surfaces was revealed \[17\]. However, in \[10; 20\] it has been noted that a cooled target has a lower failure rate compared to an uncooled one. The rate of corrosion-erosion wastage of steel in the small leak zone is substantially affected by the leak value, coolant temperature and type of material. In Fig 2.1.9 results of the experimental investigation on the failure rate of perlitic \(2\frac{1}{4}Cr\ 1Mo\) and austenitic \(18Cr8Ni\) steels a water flow rates up to 7 g/s and a sodium temperature in the range 280-530°C are presented \[21\]. Therefore, an increase of water flow rate from 0.1 to 1 g/s at a constant sodium temperature (480°C) results in an about 30 times increase of the \(2\frac{1}{4}Cr\ 1Mo\) steel failure
Fig. 2.1.8. Material destruction rate as function of $L/d_o$:
1 - $2\frac{1}{4}$ CrMo steel; 2 - OX12H2M;
3 - X18H10T steel

Fig. 2.1.9. Material destruction rate as function of water leak into sodium rate, material type and sodium temperature:
1 - CrMo steel; 2 - X18H8 steel
rate. Wastage of the same steel at a water flow rate of 0.5 g/s increases 20 times for a temperature increase from 350 to 530°C. Type 18Cr8Ni steel resistance to the sodium-water reaction products, within the considered range of leaks and temperatures, is 2.5-4 times more than that of 2¼ Cr 1Mo steel. Similar results for the dependence of the material failure rate on the leak value and sodium temperature were obtained in [16; 22; 20; 23].

For the evaluation of the corrosion-erosion wastage rate of steels in the small leak zone for heat transfer tubes interspace of 12-17 mm, the following relationships might be recommended [21]:

for 2¼ Cr 1 Mo steel

\[ W = 463 \exp\left(-\frac{0.135}{34}\left(\ln\frac{G_{\text{H}2\text{O}}}{\text{mm/s}}\right)^2 + \frac{5330}{T_{\text{Na}}}\right) \];

(18)

for austenitic type steel

\[ W = 8470 \exp\left(-\frac{0.062}{1240}\left(\ln\frac{G_{\text{H}2\text{O}}}{\text{mm/s}}\right)^2 + \frac{7520}{T_{\text{Na}}}\right) \];

(19)

where \( W \) represents the rate of steel wastage, mm/s; \( G_{\text{H}2\text{O}} \) - water-into-sodium flow rate, g/s; \( T_{\text{Na}} \) - sodium temperature, K.

Relative failure resistance under reaction products (the reciprocal of the wastage rate) of chromium(9-12% Cr), austenitic and high-nickel (35-40% Ni) steels compared with perlitic (2¼ Cr1Mo) steel can be illustrated on the basis of data from [17, 24]:

<table>
<thead>
<tr>
<th>Type of steel</th>
<th>Failure resistance</th>
</tr>
</thead>
<tbody>
<tr>
<td>2¼ Cr Mo</td>
<td>1</td>
</tr>
<tr>
<td>9 Cr 1 Mo</td>
<td>1.2</td>
</tr>
<tr>
<td>12 Cr 1 Mo</td>
<td>1.7</td>
</tr>
<tr>
<td>X18H8</td>
<td>2.5-4</td>
</tr>
<tr>
<td>Incoloy 800</td>
<td>5-6</td>
</tr>
</tbody>
</table>

A comparison of results from experiments with static and moving sodium has revealed that the coolant velocity to a certain degree affects the rate of material failure in the reaction zone. The physical nature of the process is that at a lateral contact between sodium flow and the reaction flame the axis of the latter is removed in the direction of coolant movement. Thereby the distance from a site of issue to the "target" is increased and the rate of corrosion-erosion steel wastage decreases. Fig. 2.1.10 presents the results of calculational evaluation of the sodium flow effect upon the deviation of the interaction flame axis at various distance from the place of injection (sodium velocity 1 m/s) [25]. It is seen from the figure that at a distance of \( L/d_o = 100 \) the flame deviation is about half this distance \( (r/d_o=50) \). In [18] experimental results for the rate of 2¼ Cr 1Mo steel corrosion-erosion failure (sodium velocity 0.3 - 3 m/s, sodium temperature 450°C) have been processed. A marked effect of the coolant flow velocity upon the material wastage rate has been noted [11]. Thus, an increase of sodium velocity from 0.3 to 3 m/s decreases the steel failure
Fig. 2.1.10. Jet axis relative deviation r/d₀ at 1m/s sodium velocity as function of distance from nozzle L/d₀.

Fig. 2.1.11. Steel destruction rate as function of sodium velocity and distance from leak point:

1 - ωₜₜ = 2-3 m/sec; 2 - ωₜₜ = 1.5-1.7 m/sec;
3 - ωₜₜ = 0.7-1 m/sec; 4 - ωₜₜ = 0.3-0.6 m/sec

rate by about 6 times for a target located on the coordinate L/D₀ = 40. In [12] the effect of the defect opening shape (mode of initial failure of the heat transfer tube) on the rate of failure of adjacent heat-transfer surface element material was considered. It has been shown (Fig. 2.1.12) that, for example, at a steam leak into sodium at a temperature of 460°C and moving at a velocity of 3 m/s the realization of a leak with a characteristic size of 0.4 g/s through a cylindrical opening is accompanied by a failure rate of 2⅓ Cr 1 Mo steel (the distance from the leak site being 12.7 mm) almost 2 times more compared with the leak rate through a slot under the same conditions. This effect can be explained by a decrease of the flame length due to deformation of its cross-section at a steam leak through the slot.
Very often in the heat exchange modules of the sodium-water steam generators there are inert gas spaces immediately in the region of tubeplates (BN-350, PFR). In this connection water (steam) leaks through the defects at places of tubes-to-tubeplates connections into gas spaces above the sodium level are considered as probable. In [26], independent of temperature conditions, no self-development of a water leak into gas space was experimentally found. There has been noted also no corrosion-erosion failure of tube material in a leak zone (the range of water flow rate being 0.07-0.5 g/s) that indicates a substantial difference in the mechanism of water (steam) jet interaction with sodium under the coolant level and with sodium vapour above its surface.

Some features of intermediate leaks. As pointed out above, a leak with a flow rate of some or even several tens of grams per second is classified as intermediate. Such leaks under sodium-water steam generator conditions usually correspond to initial failures with an equivalent diameter from one to several millimeters. Experimental data accumulated up to now on intermediate leaks is inferior in volume to that on smaller leaks. This is explained by the experimental difficulty of realization, and maintaining for a long time, the appropriate conditions. The intermediate leaks are characterized by the following features. Firstly, they are mainly not prone to self-development. Secondly, it is in the region of intermediate leaks where the rate of corrosion-erosion damage of material of heat transfer tubes that have proved to be under exposure to reaction products reaches a maximum, is stabilized within some range of leaks and at further increase of a leak begins gradually to decrease [21, 16, 20, 22, 23]. Thirdly, at intermediate leaks the region of material failure is not limited by the surface of a heat transfer tube adjacent to the failed one. In [21, 24, 16, 27, 28], it has been noted that the damaged zone can cover 6-10 or even a larger number of tubes. Despite the fact that a local damage of a single tube (especially in the area of leaks having a tendency to reduce the rate of material wastage) can be somewhat less compared to the small leaks, in this case a careful analysis of the tube bundle elements material state in the damage zone is needed for the purpose of determining their subsequent working capability. Fourthly, it
should be noted that it is the region of intermediate leaks where the highest rates of material corrosion-erosion wastage in the sodium-water interaction zone are realized which is characterized by the most rigid requirements on leak indication systems and some other elements of the steam generator safety system as to their speed of response.

Small leaks in the reverse-type SG. The "reverse" steam generator (RSG), in which coolant is moving within the tubes and the working fluid (steam-water) - in the tube interspace, is of particular interest as regards the character of emergency processes at a sodium-water contact. At appearance of a through defect in the RSG tube a water leak takes place into the tube with a small (compared with tube interspace) cross-section. Therefore, even in case of a relatively small leak the sodium circulation conditions in a failed tube may substantially vary due to formation of gaseous hydrogen at the sodium-water reaction. This is the cause of appreciable differences in the character of emergency processes in the once-through- and reverse-type steam generators.

An experimental investigation of the sodium flow character in a failed tube of RSG on the isothermal models with gas injection has revealed that depending on the size and site of a leak the following conditions can be realized: flowing-through, blocking (coolant does not flow through a channel filled with gas), pulse conditions (a periodic process with alternation of the flowing-through and expulsion conditions) [29, 30]. The most interesting regime from the viewpoint of an emergency process is that with cessation of coolant circulation in a leaking channel, as in this case the pattern of reactants interaction at a water leak site substantially changes.

With many parallel tubes in the steam generator design, the hydraulic changes in one of them do not significantly affect the hydraulic pressure drop between the collectors. The conditions of cessation of sodium movement in a channel in which a leak has appeared and a gas bubble of height $h$ has formed can be presented by equations:

\[
\begin{align*}
\gamma^h_{Na} &= \Delta P_D + \Delta P_{G,\text{up}}, \\
\gamma^h_{Na} &= \Delta P_D - \Delta P_{G,\text{bottom}}, \\
\gamma^h_{Na} &= -\Delta P_D + \Delta P_{G,\text{up}}.
\end{align*}
\]

where $\Delta P_D$ represents the hydrodynamic pressure drop between collectors; $\Delta P_{G,\text{up}}$ and $\Delta P_{G,\text{bottom}}$ the pressure drops created due to in-tube gaseous phase at its movement into the top or bottom collector, respectively.

Relations (20) and (21) refer to the case of sodium moving from the top downwards and correspond to formation of gas bubble in the upper (20) and lower (21) parts of a failed channel, with gas moving towards the top or bottom collector; equation (22) - to the case of coolant moving from the bottom upwards. The comparison of the above expressions shows that if sodium in a steam generator is moving downwards, blocking of a failed channel with the gas bubble formation in it can be reached at considerably less $\Delta P_G$ (lower gas-into-coolant flow rates-leak values) than for a steam generator with sodium moving from the bottom upwards. At small leaks the velocity of gaseous reaction products (hydrogen bubbles) formed does not exceed the velocity of sodium in the channel. Then an additional hydraulic resistance at the gas phase movement will be less than hydraulic resistance at sodium movement by a value proportional to the sodium-hydrogen densities ratio, and it can be neglected. For such leaks with sodium moving from the bottom upwards a loss of sodium...
flow rate in the channel (it follows from equation (22) and the formation of a stationary gas bubble in it are impossible. The relationships (20) and (21) assume the following form:

\[ \gamma_{Na}^{b} = \Delta p_D. \] (23)

From (23) it follows that with sodium moving downwards in a steam generator the appearance in a tube with a cross-section \( S_c \) of a gas volume \( V_g \) (as a result of a water-into-sodium leak) equal to:

\[ V_g = (\gamma_{Na}^{b} / \gamma_{Na}) \Delta p_D \] (24)

leads to a decrease of sodium flow rate to zero.

Knowing the site of leak location over the height of the channel, the channel cross-section and the velocity of hydrogen bubble removal by sodium flow, one can determine a minimum time required for the accumulation in a tube of the gaseous phase of \( V_g \) in volume, as well as the required gas flow rate into sodium (a type of leak) ensuring blocking conditions.

When considering the processes of leak self-development and corrosion-erosion wastage of material under the action of the reaction flame in RSG one should bear in mind that they are realized to a full extent only under conditions of sodium flowing through a failed channel.

According to [30], the time of leak self-development in once-through and reverse steam generators under flow-through conditions is the same. At the same time, the authors of work [31] point out that it is valid only for leaks less than some critical value. Thus, at a water flow rate of 0.3 - 0.5 g/s in a reverse steam generator tube of 2\(\frac{1}{4}\) Cr 1 Mo steel no self-development took place during a time up to 10^3 s that is 10-15 times more than corresponding time for the once-through design. It should be noted, however, that at present there is not sufficient experimental data to determine the value of this critical flow rate and its dependence on the inside diameter of the working channel.

It has been experimentally found that marked wastage of material of the channel wall opposite the leak, takes place only under conditions of coolant flowing through. In this case, as well as for the once-through steam generator, the rate of corrosion-erosion failure depends on geometrical characteristics of the channel, sodium temperature and velocity, water flow rate. In [32] it has been shown that under RSG conditions for 2\(\frac{1}{4}\) Cr 1 Mo steel the rate of material wastage \( \dot{W} \) can be determined from expression:

\[ W = W_0 \dot{W}'. \] (25)

Here the parameter \( W_0 \) is the rate of wastage of the same steel for the once-through steam generator obtained for conditions of sodium moving at a velocity of 0.24 m/s:

\[ W_0 = \frac{4400}{L} \exp\left(-0.255 \left( \ln \frac{G_{H_2O}}{5.12} \right)^2 + \frac{5460}{T} \right), \] (26)

where \( L \) represents the distance to the opposite wall, mm; \( G_{H_2O} \) - water flow rate, g/s; \( T \) - Na temperature, K; \( W_0 \) - the rate of wastage, mm/s.
The correction factor $W^*$ is found from the expression:

$$W^* = 1 - \left( \frac{\nu - 1}{480} \right) \frac{L}{d_0} \quad \text{at} \quad \nu < B,$$

(27)

where $\nu$ represents the relative sodium velocity (with respect to a velocity of 0.24 m/s); and $B$ the coefficient equal to 16.5 at $L/d_0 = 28$, 12.5 at $L/d_0 = 35$ and 7.9 at $L/d_0 = 68$.

In equation (27), the value $d_0$ is in correspondence with $G_{\text{H}_2\text{O}}$. At $\nu \geq B$ the correction factor $W^*$ has the following values: 0.1 at $28 < L/d_0 < 35$, 0.05 at $35 < L/d_0 < 68$, 0 at $L/d_0 > 68$.

The substantial effect of the in-channel sodium velocity on the material failure rate cannot be explained only by the deviation of the reaction flame axis and elongation, in this connection, of the distance between the leak site and the wall. Therefore, in the tests [32] it was found out that at a sodium velocity of 2.75 m/s and $L/d_0 = 68$ the displacement of the jet axis was 4.5 mm and an increase of the above distance was from 17 to 17.6 mm, with the failure rate decreasing practically to zero. It appears that with increasing sodium velocity under conditions of the confined space of the channel, mixing of pure sodium with reaction products in the flame is intensified and, as a result, the temperature and concentration of reaction products in the jet are reduced that results in a decrease of the failure rate of steel. Under expulsion conditions when the leak is in the gas bubble region no tube wall failure occurs as the leaking-out water (steam) jet does not contact with sodium. Experiments have shown [29] that in this case the temperature of the outer surface of the tube at the place of jet impingement is equal to the temperature of steam leaking out. For comparison it can be pointed out that under conditions of sodium flowing-through the temperature at the same place reached 800°C. For pulse conditions an increase of the wall temperature only during sodium flow-through is characteristic. Besides, under pulse conditions, due to thermal inertia of tube material, the wall temperature does not reach values equal to the temperature of products in the reaction flame. All this finally reduces the average rate of steel failure. In [29] it was concluded that the failure rate of steel was 3-5 times less under flow-through conditions.

One more characteristic feature of accidental conditions in the RSG channel should be noted. As is known, at water penetration into sodium there are present in it gaseous hydrogen, sodium oxide, hydride and hydroxide. It has been noted [31] that when the values of concentration of hydride, oxide and hydroxide exceed the relevant values of solubility at a temperature equal to the wall temperature their crystallization in the form of solid precipitate on the inner surface of a failed channel takes place. If drops of hydroxide not dissolved in sodium get into a temperature region below 350°C they solidify and also precipitate on the wall. The above phenomenon may favour hydraulic blocking of a RSG failed tube and thus cause self-blockage of a failed channel.

2.1.5. Large water-into-sodium leaks in steam generator

Studies of incident conditions in SG at large water-into-sodium leaks were started practically simultaneously with the development of their designs. It is explained by the fundamental importance of the above conditions for the determination of a possibility to create the heat exchange surface with single-walled separation of sodium and water. One of the first experimental large-leak studies was a series of tests carried out in USA to validate
the design of the "Enrico Fermi" NPP SG [33]. These activities gained further development after an accident at the steam generator of this NPP. The firms "Detroit Edison Co" and "Babcock and Wilcox" performed a large volume of work on the investigation of the main effects accompanying large failures of the heat exchange surface [34, 35]. Considerable work was carried out in Great Britain to validate the design of the PFR NPP SG [36]. At the Cadarache Research Centre (France) investigations as applied to the "Phenix" SG were carried out [37]. Investigations on large water-into-sodium leaks carried out later on were aimed at the determination of the scale factor (the use of larger models), further investigation of peculiarities of physical processes, as well as at the experimental verification of the computer programs developed. Among these are studies in USA, an investigation at Dounrey (Great Britain), studies at Cadarache (France) to validate main engineering solutions of the "Super-Phenix" design, Japanese studies on the "Monju" SG [38], studies in the USSR on the steam generator designs for the BOR-60, BN-350, BN-600 NPPs [9, 39, 40]. Results of recent studies carried out by scientists from various countries were reported at IAEA's meetings in the Hague (Netherlands) [41], Japan [42], Aix-en-Provence (France) [43]. Below is presented a brief review of main results from the investigation of the sodium-water reaction effects at large leaks in steam generators.

The effects of pressure variation. The appearance of a large leak at a fast and appreciable heat-exchange tube failure is accompanied by a short-time (for milliseconds) burst of pressure in the reactants' chemical interaction zone. To find out the nature of the above peak, special tests were carried out in which at one and the same pressure (6 MPa) an injection of water, water steam and gas into sodium was carried out [39]. In the first two cases the tube failure was followed by the chemical reaction, in the third case no such reaction took place. In each of the tests the tube failure process caused the formation of the shock wave but its different amplitude recorded at the wall of the steam generator model vessel pointed to the difference in the factors determining the wave parameters.

For a failure of tube by gas it is the pressure of wall rupture that forms the shock wave front. In the case of water injection, such a factor is apparently the secondary effects of a pressure rise in the reaction zone as a result of water-sodium interaction with reaction products formation. This assumption is confirmed by results of tests described in [39, 34]. In particular, it is pointed out [34] that water injection under pressure of 7.2-15.5 MPa through a rupture disk was accompanied by a shock burst of pressure up to 59 MPa at some distance from the reaction zone. Therefore, the source of the shock wave should have had even larger initial pressure. The above statements following from the experimental results are very significant for the development of the physical model of an emergency process and for the determination of the initial parameters in the reaction zone in the mathematical model.

As to a prolonged (over a few seconds) pressure rise in the failure zone and some other parts of the sodium circuit, the character of its variation is determined by the size of the defect, by hydraulic resistance of the sodium pipeline and by its inertia characteristic [39]. In Fig. 2.1.13 the results of pressure measurements at the leak site and in the gas space of the experimental rig are presented for two tests the only difference between the conditions is the different hydraulic resistance between the above zones. The curves 1 and 2 have been obtained for a test with an initial resistance of the leak site-expansion tank section of 0.08 MPa, the curves 3 and 4 - in a test with a resistance of 0.5 MPa. From the character of parameter variation, it is seen that initial short-duration pressure bursts of a shock origin in both experiments practically do not differ from one another. Further variation of pressure in the reaction zone is already different. In the first test it remains at a level of 3-4 MPa over no more than 0.3 s and then a pressure rise in the expansion tank begins, in the second
Fig. 2.1.13. Sodium circuit element pressure change as function of its section pressure drop values:
1,3 - reaction zone pressure;
2,4 - buffer vessel pressure

Hydraulic processes. It has been experimentally found that a leak of large amounts of water into sodium under steam generator conditions leads to considerable hydraulic changes in the circulation circuit sections. It is connected with the appearance of pressure gradients between the reaction zone and the gas expansion space that considerably exceed the operating ones.

An example of such a process is shown by the results of one of the tests presented in Fig. 2.1.14 [39]. A tube failure and the beginning of reactants interaction are accompanied by a marked increase of sodium flow rate $G_1$ at the leak site - expansion space section. In the inlet section of the model (failure site-pum-expansion tank) the sodium flow rate $G_2$ rapidly pump decreases, at a certain moment becomes equal to zero and can even reverse its direction of motion. Intensive movement of sodium to the gas space of the expansion tank in the both sections of the circuit causes an increase of space taken up by products of interaction in the reaction zone and, as a result, a pressure drop at the leak site despite the continued leak of water. Gradual equalization of pressures between circuit sections leads also
**Fig. 2.1.14.** Sodium flow rate change mode in circuit sections during large size leak: \( G_1 \) - at leak point-buffer vessel section; \( G_2 \) - at leak point pump-buffer vessel section

To equalization of sodium flow rates (under condition of maintaining the pump operation and the integrity of safety devices). It should be noted that under the large-leak conditions being considered a rise of sodium flow rate \( G_t \) in the outlet section of the model is accompanied by gaseous hydrogen motion from the leak site to the expansion tank. In this connection it is this section of the circuit (the interval \( yJ \) in Fig. 2.1.14) where appreciable sodium flow rate pulses can be observed. Of course, the rate of hydraulic variations in sodium circuit sections of a real NPP at a large leak in the steam generator may differ from those presented in Fig. 2.1.14 and characteristic of the test conditions under consideration, but the basic pattern will be the same.

**The temperature effects.** As noted above, the sodium-water interaction is accompanied by rather a considerable heat release (140 kJ/mole) that causes a local temperature rise in a zone of large leak appearance. It has been found by experiments that temperature bursts are of a short-duration character, pass through their maximum before leak termination and do not exceed 1400°C [39, 33, 38]. Such a character of temperature behaviour can be explained as follows. In a zone of reaction products formation the processes of intensive mixing and heat exchange between them take place. The development of a high pressure in the reaction zone leads to rapid expansion of the gaseous (hydrogen) phase and related movement of considerable masses of sodium. This results in a decrease of temperature in the reaction zone due to expansion work. Besides, there is heat exchange between reaction products and the tube bundle. An equilibrium design temperature in a large leak zone realized over its total time interval does not usually exceed 600-700°C and is determined by the capability of the sodium circulation circuit to remove the reaction products from the region of reactants' chemical interaction. At the same time, short-duration temperature peaks (as was noted above) can reach 1400°C, the temperature of the heat-exchange tube wall-up to 1100°C [44]. The consequences of such temperatures from the viewpoint of possible secondary failures of tube wall material are discussed below.

**The character of mechanical effects.** At a fast failure of the heat exchange tube and the appearance of a large leak with flow rates up to several kilograms per second no failure of the structure occurs because of a short duration of the action of rather high shock pressure bursts. The long-duration but less -amplitude pressure oscillations are taken into consideration.
Methods of calculational evaluation of steam generator parameters at a large water-into-sodium leak. For a particular postulated size of the heat exchange tube failure, its corresponding water-into-sodium leak will depend on the ratio of parameters in the steam-water circuit and sodium volume in the leak region. If it is assumed that a high pressure arising in the reaction zone does not affect the conditions of water leak (i.e., does not retard it) then the process of calculating the parameters in the steam generator circuits can be conventionally divided into two independent stages. The first of them consists in the analysis of phenomena in the steam-water circuit with account of the emergency drain system operation. Its final aim is to obtain the dependence of water (steam) - into - sodium flow rate on time through a specified damage of the heat exchange surface. At the second stage, using the leak value as an initial disturbance, the dynamic, temperature and hydraulic processes in the tube failure zone and in the elements of the sodium circuit of the plant are calculated.

Usually, when making a mathematical description of the processes in the water space of a steam generator the following assumptions are made: (a) the processes obey the adiabatic law, (b) changes in steam-water volume, steam and water mass of a steam generator are determined only by the rate of flow processes through a failure, through the emergency dump system and by the degree of self-vaporization at a pressure decrease within the volume, (c) the thermodynamic parameters of water are approximately expressed in terms of pressure in the steam-water volume, and (d) due to a lack of generalized theoretical solutions for calculating the steam-water flow motion in fairly long channels (taking into account self-vaporization of liquid) semiempirical relationships based on experimental data processing are used. The greatest difficulties for sodium circuit analysis arise due to the complexity of the processes arising there. Nevertheless, at the present time some methods have been developed that take into account the shock effects in the reaction zone, a change in the hydrogen bubble volume and reaction products temperature, in parameters of the safety system. Among these methods are the calculation code [45] and its complementary code [46] in the USSR, the TRANSWAAP-II code in USA, the calculation procedure RETONA in France, the FRG code [47]. All of them are practically based upon the same statements and assumptions (the reaction proceeds promptly, gaseous reaction products obey the laws for ideal gases, heat exchange between reaction products and structural elements is not taken into account, etc.) and differ only by some mathematical techniques and computer software.

Some attempts to take account of interaction dynamics in the reaction zone did not lead to obtaining more correct results because of the difficulty to choose the initial parameters defining the diffusion process under conditions of combined action of the chemical reactions and of complex hydrodynamic phenomena.

On the problem of design basis leak in the sodium-water SG. One of the main problems in solving the safety problems of sodium-water steam generators and sodium circuit elements, is the determination of their strength characteristics precluding loss of tightness of sodium piping in the process of an accident related with ingress of large water flow rates into sodium (the large leak). In this case the required characteristics of the steam generator safety system and the strength characteristics of the structure, closely interrelated and interdependent on each other, are unambiguously defined by the greatest perturbation (a leak) assumed at the analysis of the accidental situation under consideration (the design basis leak). At present, in world practice the experimental data on parameters of tube failures at a large
leak immediately in the reaction zone obtained under conditions close to full-scale ones are lacking. This is the cause of certain difficulties arising at the consideration of an ultimate failure size as applied to some specific steam generator design. When determining the ultimate failure extents (the character and size of a leak) the two main factors: the rate of a leak increase and the ultimate leak value, should be taken into account. A calculation analysis of the effect of the leak growth rate on the dynamic processes in the interaction reaction zone as applied to steam generator conditions is illustrated in Fig. 2.1.15. To each curve characterizing the pressure variation in the reaction zone (Fig. 2.1.15, a) its own rate of leak growth and its ultimate value are indicated (Fig. 2.1.15, b). As is seen from the plots, maximum pressure in the region of a large leak with an instantaneous increase of the water-into-sodium flow rate up to a final value of 5.6 kg/s (the curve 2) is comparable with maximum pressure obtained at an increase of the flow rate at a rate of 56 kg/s$^2$ to a level of 39.2 kg/s (that corresponds to successive failures of seven heat-transfer tubes at 0.1 s intervals, the curve 1). A decrease of the acceleration of the leak rate to 11.2 and then to 5.6 kg/s$^2$ at the same ultimate value of the leak sharply reduces maximum pressure (the curves 3, 4) as compared with the cases considered above.

The analysis presented shows an appreciable effect of the material failure rate (a leak increase) on the processes of accidental loading upon the steam generator structure, the effect of a fast (instantaneous) leak increase up to a certain value being comparable with the leak development to a considerably larger size but at a slower rate. Therefore, for a calculation analysis of dynamic effects in the sodium circuit, if one is to choose some ultimate water-into-sodium flow rate it would be desirable to assume reaching this value as instantaneous (with this assumption going into calculation margin). With a maximum calculated pressure in the reaction zone being dependent not only on the failure rate but on its value as well, it would be of interest to consider the problem of a value of the ultimate leak postulated at the accidental situation analysis. As is shown by most experimental studies, with the initial water-into-sodium flow rate of several kilograms per second (i.e., practically at a total tube

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**Fig. 2.1.15.** Large size leak zone pressure change (a) at different rates of its growth (b)
failure), in the absence of any preceding vibrational or corrosion failures of bundle material the tubes within the reaction zone, as a rule, do not fail at least during a time interval comparable with the time of the high pressure effect in the reaction zone.

It can be assumed that at a large water-into-sodium flow rate and a very fast chemical reaction between them a practically instantaneous expulsion of sodium from the failure zone takes place. Further displacement of the hydrogen-sodium interface (due to fast expansion of the hydrogen bubble) results in a constant displacement of the reaction zone. Therefore, there is no local effect of reaction products on tube bundle material, at least in the first seconds (when high pressure in the hydrogen bubble is acting) after a failure occurrence. At the same time, shock loads with a high amplitude and short-duration action prove to be insufficient for a "chain" failure of tubes.

An incident at the PFR-NPP steam superheater [48], however, made to treat more attentively tubes behaviour analysis in the large water-into-sodium leak zone in a steam generator within a time interval following the shock load effect. As a result of a leak occurrence in one tube in the process of the accident 40 tubes had a through damage - a guillotine-type rupture. According to the data from material research, the temperature in the reaction zone increased up to 1300°C. In addition to 40 tubes having through damages, about 500 tubes were exposed to high-temperature (in excess of 800°C) reaction products, on 70 tubes swelling was revealed. As a result of the analysis of the whole set of data the British specialists presented the following version of the accident development. The initial leak occurred on the tube N 16 (Fig. 2.1.16). The cause was a crack due to wall wear by about 70% of its thickness. The leak was not detected because of a lack of hydrogen-in-sodium monitoring, and the system of hydrogen monitoring in the gas space proved to be too delayed in time (it operated after a rupture of the bursting disc). Such an initial leak could continue for several hours at a flow rate of 0.001 g/s and a few minutes at a flow rate of 1.0 g/s. Then the leak passed into an intermediate one and during about 20 s was at a level of 0.5 kg/s. The consequence of this leak was a guillotine-type rupture of a few adjacent tubes weakened by abrasion wear due to vibration and by their being in the high-temperature zone. The cause of the rupture was a high steam pressure (13 MPa). The rest of the tubes were damaged in the process of steam generator shut down when after the cessation of water and steam flow rate the heat removal from the reaction zone was stopped. The process was aggravated by an inadequate design of the sodium and reaction products removal system. Altogether, about 150 kg of water penetrated into sodium. Thus it has been found that at an initial large leak, under some conditions the secondary failure of heat exchange tubes is possible because of metal overheating.

Experiments carried out by USA specialists [44] have confirmed the above fact. Thus, one of the tests was carried out at an initial leak of 454 g/s. The first of the secondary leaks appeared on the target tube in 16 s and was caused both by corrosion and by a rupture under the effect of internal pressure. The traces of corrosion wastage were found on one more tube. All the other failures (a total of 23 tubes were failed) were of a "overheating-rupture" character (Figs. 2.1.17, 2.1.18). It was recognized that most of the secondary failures were the result of the reaction between steam leaking from the failed tubes and residual sodium after its draining. An important result of the experiments is also that despite a considerable number of damaged tubes the acoustic waves of pressure were determined by the initial large leak.

On the basis of a generalized analysis of the results of the experimental validation studies carried out on the EFR design [19, 27, 49] it has been found that the effect of a tube
Fig. 2.1.16. Cross section of the PFR steam generator tube bundle with failed tubes (figures show tube numbers)
Fig. 2.1.17. LLTI TUBE ARRAY - TEST A-5
Typical tube failures and temperatures
wall failure by the "overheating-rupture" mechanism is possible at an appearance of the secondary (or of the primary) leak with a flow rate more than 80 g/s. And in this case the time from the appearance of this large leak up to a failure of adjacent tubes depends on the leak size. In Fig. 2.1.19 it is shown that with an increase of a leak from 100 g/s to 4000 g/s the above time interval reduces from 50 to 4 s. Thus, when developing an approach to the choice of a design basis leak value the following should be taken into account: (a) the rate of hydrodynamic processes in the sodium circuit (and, therefore, the circuit strength) is determined by the rate of an increase of an initial large leak. It is expedient, therefore, as an initial leak to take that appearing at an instantaneous guillotine failure of at least one tube, and (b) a steam generator tube bundle damage zone (the number of heat exchange tubes), as well as the amount of water penetrating into the sodium circuit (and, therefore, the scope of repair work) depend on the SG design features and on its safety system characteristics. It is desirable to aim at the most rapid possible termination of the sodium-water interaction reaction after the appearance of the secondary (or the primary) large leak.
2.1.6. Steam generator protective system

The protective system and its relation with a SG design. To clear up a question of the interrelation between the protective system (PS) and a sodium-water SG design it is essential to note some characteristic features of the design for structure integrity (SI) assurance under incident conditions. Such features may include as follows: a design and engineering solution; a scheme of modules connection; a heat-transfer surface design; and an arrangement of coolant and working fluid motion. Of course, each of the above characteristics is to be taken into account at the protective system design. Nevertheless, depending on the combined design criteria, two basic ways of solving the sodium-water SG - SI assurance can be envisaged: (1) at a simplified SG design with single-walled separation of coolants a sophisticated PS is used ensuring the required SI and performance of the steam generator under an assumption of a
particular frequency of emergency situations, and (2) a sophisticated steam generator design reducing to a minimum a possibility of a sodium-water contact (the use of multiwalled heat transfer surfaces) is combined with a simplified PS design. However, irrespective of the type of the steam generator design used, its SS should include the following main subsystems of varying complexity: for leak indication and signal shaping; sodium and water circuits SI. In single-walled designs (integral and sectional, once-through and reverse) signal shaping should be based on recording some indications characterizing the change of sodium circuit parameters under "water-into-sodium leak" conditions. For a large leak these are the effects of a change in pressure, flow rate, temperature, level; the acoustic noise. For a small leak the variation of impurity content in gas spaces and in sodium; fluctuations of sodium flow rate; acoustic (and, perhaps, thermal) noise.

The present concept of solving the problems of sodium-water steam generators reliable operation is to ensure that, from the moment of detecting a leak (with a contact of coolants or without it) in a failed structural element, the simultaneous presence of coolant and working fluid should be precluded. From the engineering point of view, fast draining of the steam-water space would be more convenient. In this case each SG should have also the pressure relief system. The latter usually includes rapid-acting valves on the pipelines of feed water, superheated steam, on dump pipelines, as well as expansion tanks for the steam-water mixture. For sectional designs there are needed rapid-acting devices that are capable of draining the steam-water space of a failed section only. Steam generators irrespective of their heat-transfer surface design should have a safety system incorporating, for the sodium circuit, the damping gas spaces, protection (rupture) discs, devices for reaction products removal and separation.

Requirements on the protective system "small leak" operating conditions. When considering the requirements on the protective system and determining criteria of its efficiency under "small leak" conditions one should note that in the absence of dynamic, temperature and hydraulic effects (characteristic of a large leak) primary attention should be given to timely detection of the small leak and, wherever possible, to prevention of damage within the reaction zone. In so doing the interrelation between the following characteristics should be taken into account: corrosion-erosion wastage of material at various flow rates of water leaking into sodium; elements of the indication system intended for monitoring of small leaks.

This interrelation should be such that, in the limit, the following requirements are met. For the integral SG design a leak should be detected and water-into-sodium penetration stopped until the moment when corrosion-erosion wastage of steam generator elements is still within allowable limits (as regards the leak size). For the sectional design at the moment of receiving the signal of the leak occurrence, the reliable indication of a failed section should be provided; the failed section after its detection should be isolated from the main circuit and from the steam generator sections left in normal operation prior the moment of small leak changing over to the large one.

"Large leak" operating conditions. Based on the effects characteristic of the large leak the SG protective system should meet the following requirements: (1) timely shaping of a signal for any steam generator type and reliable detection of a failed section for the sectional steam generator type, in particular, should be provided; (2) the PS should prevent a pressure increase above an ultimate permissible value in the steam generator, in the failed section being cut off (for the sectional type) and in the sodium circuit elements, and (3) the relative PS elements should ensure the required speed of cutting off of a failed section in the
sectional steam generator to prevent considerable contamination of coolant of the main circuit under the "large leak" conditions. In relation to the large leak detection problems (the requirement 1), by analogy with the problems of small leak detection, it is necessary to analyze the possibility of shaping the signal on the basis of changes of parameters in the sodium pipeline and in the gas spaces. In this case the most characteristic parameters for the gas spaces may be pressure, temperature and the level of sodium, and for the gas spaces-pressure, flow rate of coolant and its temperature.

Sodium-water SG - PS designs. Single-walled SG designs have come into use everywhere (the facilities BOR-60, BN-600 in Russia, "Phenix" and "Super-Phenix" in France, PFR in Great Britain, BN-350 in Kazakhstan, FBTR in India, and from the viewpoint of design, the steam generator PS of various plants have much in common.

The subsystem for shaping the signal at small leaks, practically in all the designs realized, is based on using hydrogen-in-gas and in-sodium detectors. In this case there is often used an arrangement with detectors location in each gas space (if there is any, in the BN-350, PFR, "Super-Phenix" modules, Figs. 2.1.20-2.1.22) or in the bulk volume of the expansion tank ("Phenix", BN-600, Figs. 2.1.23-2.1.24). Hydrogen-in-sodium detectors can be mounted on each module (PFR), one per group of modules (BN-600, "Phenix") and one per SG (BN-350, "Super-Phenix").

Fig. 2.1.20  Schematic representation of the BN-350 steam generator protective system: 1 - evaporator; 2 - superheater; 3 - gas blanket; 4 - first stage separator; 5 - rupture disc in gas blanket; 6 - second stage separator; 7 - steam/water fast actuating valves; 8 - emergency water discharge line; 9 - expander; 10 - feed water supply pipe; 11 - superheated steam outlet; 12 - hydrogen in gas detector; 13 - hydrogen in sodium detector; 14 - gas blanket pressure indicator
Minor leaks in steam generators are detected by hydrogen detectors operating on the principle of nickel membrane systems. They are used for both hydrogen detection in the sodium and in the argon cover gas of the steam generator. The response time of these hydrogen detectors is a function of leak size and the transport time of hydrogen from the reaction zone to the detector. The response time will be of the order of minutes in the case of very small leaks. Acoustic detectors with wave guides eliminate the problem of long transport times. Medium and large size leaks with ensuing sodium-water interactions are detected by pressure sensors and the destruction of the rupture discs. In addition, the inrush of sodium into the pressure suppression lines is detected by contact sensors [51]. Besides, there is considered a possibility of detecting leaks with a flow rate at a level of several fractions of a gram per second and more with the use of systems based upon using an effect of electric signal pulse generation at hydrogen bubbles passing in the sodium flow through a detector. As the latter either the magnetic flowmeter or induction device are used. The above-indication systems are simple in fabrication and operation, convenient for leak indication in the multimodal steam generator arrangement (per-module monitoring). However, the degree of their effectiveness depends on hydrogen-in-sodium dissolution.
Fig. 2.1.22. Schematic representation of the PFR reactor NPP steam generator protective system:
1 - evaporator; 2 - superheater; 3 - reheater; 4 - 200mm diameter rupture discs on sodium piping; 5 - 300mm diameter rupture discs on sodium piping; 6 - 300mm diameter rupture discs on gas piping; 7 - discharge header; 8 - first stage separator; 9 - second stage separator; 10 - check valve; 11 - rupture disc; 12 - fast actuating steam/water valves; 13 - expanders; 14 - detector of hydrogen in sodium. 15 - detectors of hydrogen in gas blanket

conditions [50]. Shaping of the signal under "large leak" conditions takes place either by an increase of pressure in the sodium circuit (BN-350, BN-600 where at an initial pressure of 0.15-0.25 MPa an alarm setting is at a level of 0.25-0.35 MPa) or at an emergency rupture of the disc in the sodium space and closing a contact at a coolant leak ("Phenix", PFR, "Super-Phenix") or by monitoring sodium flow rate deviations at steam generator sodium pipelines (BN-600). Protective subsystems on the steam-water circuit in all the steam generators under consideration have much in common. Most use a scheme of emergency draining of the steam-water volume into one common expansion tank (for example, BN-350, "Phenix"). An exception is the steam generator of the PFR-NPP where steam and water from each module are discharged into a separate expansion tank. In PS circuits rapid-acting valves with pneumatic, hydraulic or electric drives are used. The response time is 0.5-20 s. The most diversity is observed in the realization of the safety system on the sodium circuit.
The damping gas spaces are located both directly in the modules (BN-350, PFR, "Super-Phenix") and in the remote tanks as well (BN-600, "Phenix").

Rupture discs are used either only in gas spaces (BN-350, BN-600), or in a combination of discs operating in gas and sodium (PFR, "Phenix", "Super-Phenix"). In the USSR the principles of forced blasting of discs in the gas spaces (BN-350) or of spontaneous one (BN-600) are used. In other versions a spontaneous rupture related to an emergency pressure increase is used. Rupture pressure at a spontaneous rupture is 0.3-1.5 MPa. Forced blasting takes place at a pressure increase from the initial one by 0.05-0.1 MPa. The diameter of the discs in use is within 150-300 mm. The arrangements used for reaction product discharge and separation are two-staged almost everywhere. There are used either two tanks where the separation of products is based on the variation of the flow motion direction and velocity (BN-350, BN-600), or one of the stages is cyclone separator (PFR, "Super-Phenix").

Devices used for hydrogen discharge to the atmosphere are either hydroseals in combination with check valves (BN-350), or the valves only (BN-600), or rupture discs in combination with check valve ("Phenix", PFR). Most of the steam generators have no valves on the sodium circuit (BN-350, "Super-Phenix", PFR). At the inlet and outlet of the steam generator of the "Phenix" NPP there is a valve 450 mm in diameter with a speed of response 6 s. At the BN-600 NPP there are valves on each group of modules (the sections) mounted on the inlet and outlet ducts Dy 350. Their time of response is 60 s.
Fig. 2.1.24. Schematic representation of "Super Phenix" NPPs steam generator protective system:
1 - steam generator; 2 - gas blanket; 3 - rupture disc in gas blanket; 4 - rupture discs on sodium piping; 5 - first stage separator; 6 - second stage separator; 7 - sodium leak detector; 8 - detector of hydrogen in gas; 9 - detector of hydrogen in sodium; 10 - selector of sodium sampling points; 11 - emergency water discharge; 12 - sodium in; 13 - sodium out; 14 - feed water supply; 15 - superheated steam out

Despite the fact that the steam generators of the "Phenix" NPP and of the BN-600 consist of a group of modules connected in parallel the valves mounted are not used for cutting off a failed section "in operation" while maintaining in service (if necessary) intact elements at each leak type. The fact is that a scheme of SS chosen at these steam generators does not assure timely localization of the reaction zone under the "large leak" conditions with simultaneous assurance of failed and serviceable modules safety. However, in case of a small leak passing over into a large one, an algorithm with shutting down the whole steam generator is also operating.

At present for the steam generators of the currently designed BN-800 NPP the PS (Fig. 2.1.25.) is under development that would allow fully to use the advantages of the sectional designs. At these steam generators the medium capacity modules connected in parallel are combined into sections in such a way that cutting off of one or two sections would not seriously affect the thermohydraulic conditions of the remaining ones. The safety subsystems on sodium and water (steam) use a collector concept with a possibility of
Fig. 2.1.25. Schematic of BN-800 NPP steam generator protective system.

1 - evaporator; 2 - superheater; 3 - fast actuating sodium valves; 4 - reaction products discharge pipings; 5 - discharge header; 6 - first and second stage separators; 7 - rupture disc in sodium; 8 - rupture disc in gas; 9 - duplication discharge line; 10 - fast actuating water/steam valves; 11 - section gas blanket; 12 - water/steam discharge valve; 13 - buffer vessel; 14 - detector of hydrogen in gas; 15 - detector of hydrogen in sodium; 16 - sodium flow rate indicator; 17 - detector of hydrogen bubbles in sodium flow.

Switching over of sodium and steam-water spaces of a failed section from standard pipelines to the dump systems. In this case it would be possible to use one standard algorithm of safety system operation irrespective of the leak type, to prevent the propagation of emergency effects from a failed section to the sodium circuit, as well as to retain in operation (if necessary) intact steam generator elements.

**CONCLUSIONS**

Some experimental and prototype NPPs with sodium-cooled fast reactors had water/steam-sodium contact in SG. This reflects the fact that the requirements on sodium-water SG tightness of the heat-exchange surface. In this case the requirements on sodium-water steam generator tightness should be far higher than for any other heat exchanger. When considering the specific problems, on the basis of experience obtained on some NPPs the following conclusions can be drawn:

1. Operating experience of the "Enrico Fermi" NPP - SG (the first SG with single-walled separation of sodium and water) has shown that tubes vibration led to their very fast failure and, as a result, to large leaks. Insufficient attention was given to design optimization of some units and systems. It was also concluded that the choice of a heat exchange tube wall thickness of 1 mm proved to be an inappropriate solution and cannot be adopted for the sodium - water steam generators.
2. As is known, the cause of a large number of large and small water leaks into sodium at the BN-350 NPP steam generators in 1972-1975 was their poor quality of fabrication. Nevertheless, the experience gained from incident at the BN-350 NPP steam generators has also some positive results. Firstly (and this is most important), it was shown that in principle an ingress of even large quantities of water into sodium (in one of the incident about 800 kg of water got into the sodium circuit) does not lead to catastrophic consequences. Therefore, integration of sodium and water under single-plant conditions is quite justified from the engineering point of view. Secondly, steam generators and sodium circuits, even under conditions of substantial water -into- sodium penetrations, remain repairable. Thirdly, the steam generator safety systems, even under conditions of severe emergency situations, are capable of ensuring plant safety.

3. Water-into-sodium leaks that occurred at steam generators of the PFR and "Phenix" were of a much less scale than at the BN-350 NPP. However, these leaks have also confirmed, from the viewpoint of emergency conditions caused by chemical activity of sodium to water, the main conclusions drawn on the basis of the BN-350 NPP experience.

4. Operating experience of the BN-600 NPP steam generators has revealed the necessity of a more careful choice of materials and refinement of fabrication techniques (in particular, of welded joints). But the main conclusion is that for the sodium-water steam generator there can be developed such a scheme that would be practically fully capable of precluding the effect of leaks in the steam generator on the operation of the plant as a whole. In this case the question is of the sectional -modular scheme, where 24 single modules of the evaporator, reheater and superheaters are integrated into eight sections each of which can be cut off while maintaining in operation all other sections. Of course, such a scheme is more expensive as compared with the integral one. However, during 15 years of commercial operation this scheme fully paid for itself. From our point of view near future activities should be arranged on optimization of the scheme, heat- exchange modules design and safety system of the sodium - water steam generators taking into account the operating experience gained with the steam generators themselves and with other power equipment as well. The optimization criterion should take account of economic expenses due to damage related to situations at water leaks into sodium. There can be also no doubt that such work should be carried out on the basis of international cooperation under the auspices of IAEA.

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2.2. SODIUM-WATER CONTACT IN THE STEAM GENERATOR BUILDING

In the case of sodium and water interaction inside the steam generator, chemical reaction products such as sodium hydroxide, hydride and oxide should be reliably localized within the safety system, and as for hydrogen, it should be removed from the steam generator cell. Since the safety system reliability is rather high one can assume that the hydrogen appearance inside the steam generator cell is unlikely. Other ways of hydrogen production such as direct sodium and water contact in the air filled cell are also of low probability. In view of this, failures resulting in the hydrogen-air mixture formation in the cells where both sodium and water-steam piping are situated haven’t been considered in the designs of advances fast reactors. The authors do not have any information about such type of research performed by the specialists in other countries. Their general considerations on this studies are represented briefly below in relation to the BN-800 reactor which is under development and construction in Russia. The analyses have revealed the following scenario that could result in hydrogen appearance in the steam generator cell: steam generator tube leak resulting in the module wall burn-through and sodium-water interaction products (including hydrogen) release into the cell.

2.2.1. Transient parameter appreciation methods

Current physical models of hydrogen-air mixture formation and combustion and analytical models created on this basis have used experimental data. Hydrogen production unit operating experience data, results of ignition temperature and explosive limit study have been published in [1, 2, 3, 4]. Results of comprehensive experimental research of flame propagation rate in the hydrogen-air mixture combustion process have been presented in [5, 6, 7]. So called empirical models of the combustion process [8] are rather simple. It is assumed in this model that combustion products are uniformly distributed in the unburnt mixture and so the flame front movement is not taken into account. It is assumed that the cell where the hydrogen combustion takes place is filled with steam, air and hydrogen, masses of which are respectively: \( M_{S(0)} \), \( M_{A(0)} \) and \( M_{H_2(0)} \).

In order to determine the possibility of combustion of hydrogen mixed with steam and air, the mixture components content ratio is compared to the hydrogen combustion content limit which are taken from Fig. 2.2.1. diagram according to [9]. It should be noted, that in 4 to 10 vol. % content range hydrogen does not burn up completely, as shown in Fig. 2.2.2. [10]. Taking into account the principal propositions presented in [8, 9, 10], the combustion volume pressure and temperature change calculation method is proposed in [11]. Component mass changes in the combustion process are calculated according to the following chemical reaction relation:

\[ 2 \text{H}_2 + \text{O}_2 = 2 \text{H}_2\text{O}. \]  \hspace{1cm} (1)

Taking into consideration that 8 kg of oxygen are needed to burn 1 kg of hydrogen, combustion component ratio can be written as follows:

\[ \Delta M_{H_2}^b = \gamma_{H_2} M_{H_2} \quad \text{with} \quad M_{O_2} \geq 8 \gamma_{H_2} M_{H_2}. \]  \hspace{1cm} (2)
Fig. 2.2.1. Combustion and detonation limits for hydrogen-air-steam mixture; 1 - combustion limits:

--- 24°C, 0.1MPa; ---- 150°C, 0.1MPa; --- 150°C, 0.8MPa; 2 - detonation limits

Fig. 2.2.2. Hydrogen burning completeness in hydrogen-air-steam mixture; --- no steam; ---- 15% of steam
\[
\Delta M_{H_2}^b = M_{O_2} / 8 \quad \text{with} \quad M_{O_2} < 8 \gamma_{H_2} M_{H_2},
\]

where \(\Delta M_{H_2}^b\)

represents the hydrogen mass burnt,

\(\gamma_{H_2}\) - hydrogen combustion extent.

Then the mixture mass and heat balance equations are written with due regard for \(M_{H_2}, M_A\) and \(M_S\) component mass change. It is assumed that the mixture components are in thermodynamic balance condition. But air composition change (\(O_2\) and \(N_2\) content ratio) and combustion product dissociation at the oxygen combustion are not taken into account.

Reaction product pressure is determined as the sum of mixture component partial pressures at given temperature, and hydrogen and air partial pressure values are calculated using the ideal gas state equation. The following equation is proposed for the steam partial pressure calculation:

\[
P_p = P_s \frac{T}{T_s} \frac{\varepsilon}{e},
\]

where \(P_s\) and \(T_s\) - steam saturation pressure and temperature corresponding to the steam specific volume in the cell; \(\varepsilon e\) - correction factor accounting for the steam and ideal gas property difference; \(P_s, T_s\) and \(\varepsilon\) values are calculated using specially selected empirical relationships [1].

The gas-steam media temperature for hydrogen combustion is obtained from the energy balance equation with due regard for combustion heat. Besides, in order to determine component internal energy values, relationships obtained by the experimental data approximation are used [12, 13]. In the frame of this method, hydrogen combustion process is referred as prompt and adiabatic, therefore pressure and temperature values obtained are maximum possible.

The parameter calculation method for the homogeneous hydrogen-air mixture combustion process, with due regard for flame propagation rate, has been developed by IPPE using principal propositions represented in [5]. It should be noted that the combustion process is considered in connection with continuous hydrogen inflow resulted from sodium and water interaction. There are two phases of the process. In the first phase reagents interaction takes place resulting in the hydrogen release into the air; it is assumed that there is no hydrogen ignition in the interaction area. Volume hydrogen concentration value achieves the lower ignition limit. In the second phase hydrogen-air mixture combustion occurs. The combustion process can be represented by the following relationships:
\[ \text{H}_2 + \text{O}_2 \rightarrow \frac{1}{2} \text{H}_2 + \text{OH} \]

\[ H_2\text{O} = \frac{1}{2} \text{H}_2 + \text{OH} \]

\[ \frac{1}{2} \text{O}_2 + \frac{1}{2} \text{N}_2 = \text{NO} \]

i.e. the dissociation process is taken into account.

The volume pressure change at every time interval is defined by the gas mixture molar composition ratio and its temperature. Mixture molar composition, with due regard for dissociation, can be represented as follows:

\[ \sum m(\tau) = m_{H_2O}(\tau) + m_{H_2}(\tau) + m_{O_2}(\tau) + m_{H}(\tau) + m_{O}(\tau) + m_{OH}(\tau) + m_{N_2}(\tau) + m_{NO}(\tau) \]

Liquid metal and water reaction process is considered in order to determine the hydrogen mole quantity. The equation system describing chemical reaction balance ratios and dissociation reaction equilibrium constants is used for the mixture molar composition determination.

When considering thermal processes, sodium and water reaction heat as well as hydrogen-air mixture combustion heat, are taken into account. Assuming that the heat is used for the dissociation process and combustion product temperature change, mixture temperature can be found from the system of equations describing dissociation specific heat and component internal energy as functions of temperature. The method takes into account loss of the volume integrity. The process of mixture release through the wall breach is described by the transient hydrodynamic equation, and volume component mole composition is determined with due regard for the release process. It is assumed that the volume pressure rise is directly proportional to the mixture burn up extent (this assumption is correct and widely adopted provided the burn up takes place uniformly in the volume with given constant rate). Thus parameter calculation methods have been discussed concerning homogeneous hydrogen-air mixture burning. Nevertheless it is more probable for the mixture to achieve explosive condition in some local area adjacent to the hydrogen source. Assuming that the mixture burns up promptly in this local area volume and the maximum pressure value is achieved, several methods can be used for determination of the propagating wave front pressure at some distance from the burning epicenter.

For example, a rough approximation can be made using the acoustic approximation [14], that determines time dependent pressure at the volume point having coordinate \( r \):

\[ P(r, \tau) = P_m + \frac{r(\tau')}{r} [P(\tau') - P_m] \]

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where \( P_m \) - media pressure, \( r_{v} \) - mixture burning local volume radius, \\
\( \tau' = \frac{r - r_s(\tau)}{s} \); \\
s - sound velocity.

It should be kept in mind that this method results in some pressure value overestimate since compressed gas layer energy losses are not taken into account.

The more correct results can be obtained using point explosion solution for the gas having constant initial density value [15]. In this case shock wave front pressure will be determined by the energy \( E \) concentrated in the initial volume, the symmetry type (parameter \( \nu \)) and the wave coordinate \( r \):

\[
P = \frac{8E}{(\nu + 2)^{2}(k + 1)} \cdot \frac{1}{r^{\nu}},
\]

where \( \nu = 1 \) for plane, \( \nu = 2 \) for cylinder, \( \nu = 3 \) for sphere, \( k \) - adiabatic constant.

Nevertheless, the authors believe that, the most complete and accurate solution of the problem of spatial variation of determination of pressure values at local explosive mixture \( \text{H} \) combustion has been obtained by Moscow Technical Building Institute (MTBI) on the basis of comprehensive experimental data treatment. The method of maximum positive pressure value determination in a passing shock wave caused by external explosions was presented in the Explosion protection building and structure design regulations draft, first developed in 1988 by MTBI. It is assumed that the explosive mixture is concentrated in a certain volume (cloud). Considering hydrogen-air mixture formation in the steam generator cell this mixture "cloud" can be referred as an initiating event version conformably to the case of local explosive concentration. If this locally concentrated mixture volume is small in comparison with the whole cell volume, then the method of passing shock wave pressure determination (external explosion) can be used for approximation of pressure values in the cell areas remote from the local explosive mixture volume. The principal propositions of MTBI method are as follows: maximum positive pressure at given normalized distance \( R^o \) is calculated by the following expression (coefficients - Tables 2.2.1 and 2.2.2):

\[
\Delta P_m = \Delta P_{\text{max}} \times \frac{1 + B (R^o + 1)^c}{1 + B (R^o + 1)^c},
\]

where \( \Delta P_{\text{max}} \) represents the maximum possible positive pressure at given explosion burning rate and at \( 0 \leq R^o \leq 1 \):

\[
\Delta P_{\text{max}} = \begin{cases} 
2.1 P_o \frac{\gamma^2}{1 + \gamma} & \text{at } \gamma < 0.7; \\
2.27 P_o \frac{\gamma^2}{1 + \gamma} & \text{at } 0.7 \leq \gamma \leq 0.8;
\end{cases}
\]

\[
\gamma = \frac{U_s}{S_o}, \quad R^o = \frac{R}{R_c};
\]

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R - distance from explosion epicenter, m,
Rc - radius of explosion cloud occupied by combustion products, m,
Us - flame propagation rate, m/s,
S - sound velocity m/s,
P0 - atmospheric pressure, kPa,
B and C - respectively dimensionless factor and exponent.

### TABLE 2.2.1. B AND C COEFFICIENTS VALUES AS FUNCTION OF γ

<table>
<thead>
<tr>
<th>γ</th>
<th>0.2</th>
<th>0.3</th>
<th>0.4</th>
<th>0.5</th>
<th>0.6</th>
<th>0.7</th>
</tr>
</thead>
<tbody>
<tr>
<td>B</td>
<td>0.588</td>
<td>0.567</td>
<td>0.555</td>
<td>0.546</td>
<td>0.467</td>
<td>0.499</td>
</tr>
<tr>
<td>C</td>
<td>1.144</td>
<td>1.077</td>
<td>1.085</td>
<td>1.048</td>
<td>1.140</td>
<td>1.118</td>
</tr>
</tbody>
</table>

### TABLE 2.2.2. FLAME PROPAGATION RATE VALUES

<table>
<thead>
<tr>
<th>Explosion conditions</th>
<th>Us, m/s</th>
</tr>
</thead>
<tbody>
<tr>
<td>1. Explosion, resulting in high rate combustible release in high pressure and temperature production units having turbulence stimulators such as weaved tubes, structures, stacks, perforated barriers etc.</td>
<td>240/200</td>
</tr>
<tr>
<td>2. The same as 1, but with no turbulence stimulators.</td>
<td>200/170</td>
</tr>
<tr>
<td>3. Explosions of flow mobil clouds at the presence of turbulence stimulators such as stacks, weaved tubes, metal structures, perforated barriers, trestles etc.</td>
<td>170</td>
</tr>
<tr>
<td>4. The same as 3, but with no turbulence stimulators at combustible mixture (CM) volume:</td>
<td></td>
</tr>
<tr>
<td>more than 210^4 m^3</td>
<td>140</td>
</tr>
<tr>
<td>510^3 to 2010^3 m^3</td>
<td>100</td>
</tr>
<tr>
<td>less than 510^3 m^3</td>
<td>85</td>
</tr>
<tr>
<td>less than 500 m^3</td>
<td>65</td>
</tr>
</tbody>
</table>

Note: figures in ratio numerator correspond to high combustion rate substances such as hydrogen, acetylene, propylene and ethylene.

As it has been shown above, R and Rc values are needed for normalized distance R° determination. Rc value is calculated by the following expressions:

\[
R_c = 0.62 \sqrt[3]{V_{cm}} \cdot \varepsilon \quad \text{at} \ R_c \leq h_{eh},
\]

\[
R_c = 0.783 \sqrt[3]{V_{cm}} \cdot \varepsilon \quad \text{at} \ R_c > h_{eh},
\]

(9)
where \( h_{eh} \) - explosion vertical height, m; \( V_{cm} \) - CM volume, \( m^3 \); \( \varepsilon_e \) - calculated combustion products thermal expansion degree.

CM volume is calculated by the following expression:

\[
V_{cm} = \frac{2 \cdot m_c \cdot z \cdot 1000}{(c_{lc} + c_{st})},
\]

(10)

where \( m_c \) - combustible mass, forming CM, kg;
\( z \) - factor of combustible participation in explosion;
\( c_{lc} \) - combustible mass concentration value in CM, corresponding to lower concentration limit of flame propagation, g/m\(^3\);
\( c_{st} \) - mass stoichiometric combustible concentration in CM, g/m\(^3\);
\( c_{lc} = 3.5 \text{ g/m}^3 \) and \( c_{st} = 24.7 \text{ g/m}^3 \) values can be taken for hydrogen.

Combustion product thermal expansion degree is calculated by the following expression:

\[
\varepsilon_e = 0.5 (\varepsilon_{elc} + \varepsilon_{est}),
\]

(11)

where \( \varepsilon_{elc} \) represents the thermal expansion degree of combustion products of CM having combustible concentration value corresponding to the lower concentration of flame propagation; \( \varepsilon_{est} \) - the same at stoichiometric combustible concentration; \( \varepsilon_{elc} = 2.1 \) and \( \varepsilon_{est} = 6.9 \) values can be taken for hydrogen.

Thus, above presented method of volume pressure determination in the case of explosive mixture combustion enables definition of the pressure value at the given cell point remote from local hydrogen-air mixture.

Calculation results of combustion process of homogeneous and local hydrogen-air mixture formed in the sodium heated steam generator cell (of BN-800 reactor NPP type) according to scenario are presented below as an example.

The steam generator cell sketch is presented in Fig. 2.2.3. It is divided conventionally into two symmetric parts each of which is divided into four communicating volumes \( (V_1, V_2, V_3, V_4) \). Module wall burn-through and hydrogen accumulation take place in \( V_1 \) volume. If the ventilation system operates properly no explosive hydrogen-air mixture is formed. If the ventilation system is out of operation then lower explosive limit mixture (4 vol.% of hydrogen) formation is only possible on condition that hydrogen is released into the cell atmosphere during a sufficiently long time (~ 1700 s).

Pressure change in different cell volumes during homogeneous mixture 250 m/s rate combustion process taking place in volume \( V_1 \) has been calculated using the method developed by IPPE; the results are presented in Fig. 2.2.4. The maximum pressure value change in the cell point at 10 m distance from the combustion center as a function of the volume of the local explosive mixture has been calculated by the method developed by MTBI. Calculation results are presented as example in Fig. 2.2.5.
Fig. 2.2.3. Elevation of steam generator building sodium system area:
1 - buffer vessel area; 2 - buffer vessel; 3 - superheater module; 4 - maintenance platforms; 5 - place of superheater vessel wall burn-through; 6 - evaporator module; 7 - emergency discharge vessel-1 area; 8 - emergency discharge vessel-1

Fig. 2.2.4. SG building parameters change at homogeneous mixture combustion rate $W_c = 250\text{ m/sec}$

at $T=0$: $P_1=P_2=P_3=P_4=1\text{ bar}$ $W_c = 250\text{ m/sec}$
Fig. 2.2.5. *Cell maximum pressure value at 10m distance from combustion centre as function of local explosive mixture volume*

**REFERENCES**


2.3. SODIUM FIRES

2.3.1. Sodium combustion process features

Metallic sodium in its pure form does not exist in nature. The reason for this is its high chemical activity, especially with respect to air and water. Sodium interacts with water very intensively in the open air. In contrast the sodium reaction with air is slow. Sodium is solid at normal temperature, its surface is coated with oxide. It is not possible to determine the exact value of sodium ignition temperature because it depends on impurities content in sodium, air humidity and the sodium-air interface condition. According to the results of studies performed by different investigators the ignition temperature values lie in the range 140 to 320°C.

As for combustion process intensity, sodium is at one of the last places among other combustibles. This can be illustrated by comparing sodium and petrol pool combustion characteristics [1, 2]. Fig. 2.3.1 represents temperature distribution in the area above burning sodium and petrol. Combustion conditions were equal in both cases, burning surface area was about 1 m². Sodium combustion rate was 45 kg/m² hour. The temperature at 1 m height above the sodium surface was lower than 100°C. Reaction zone (flame zone) was situated close over the sodium surface. In the case of petrol, the flame was as high as 4 m. Time average temperature at 2 m height above the petrol surface was more than 600°C, petrol combustion rate was 4 times more than that of sodium. This is owing to different physical properties of sodium and petrol. Sodium boiling temperature is 880°C at atmospheric pressure, and evaporation latent heat value is 4340 kJ/kg. This results in its relatively low evaporation rate. On the other hand petrol boiling temperature is 80°C and its evaporation latent heat value is 12 times less compared to that of sodium. Therefore burning petrol achieves its boiling point soon which results in sufficient vapour over the petrol surface. That is why a high

![Temperature distribution in the area above burning sodium and petrol](image-url)
flame is formed over the petrol. Sodium-air reaction heat is 10900 kJ/kg, that is 4 times less in comparison with petrol. Thus, energy release rate during sodium combustion is approximately 15 times less than that in the case of petrol.

The intensity of processes in the reaction zone is determined by two mechanisms: (a) air oxygen diffusion into reaction zone, and (b) opposite direction sodium vapour diffusion from sodium surface into the reaction zone.

The first mechanism basically controls processes at temperatures lower than 650°C. With temperature increase the second mechanism becomes more and more significant. The higher the sodium temperature the more the vapour generation. The reaction zone withdraws from the sodium surface resulting in decrease of heat transfer to the sodium. This, in its turn, decreases the evaporation rate. Studies have shown that stationary state is achieved at the pool sodium temperature of 720 to 745°C. The flame area temperature is about 200°C higher, sometimes it can reach 1000°C. During the combustion process 15 to 25% of the combustion product mass leaves the reaction zone in the form of fume. Nevertheless sodium mass stays almost constant because of simultaneous mass increase due to sodium oxidation. All this concerns combustion of sodium spilled over the horizonted plane. However sodium drop combustion can also take place. Combustion specific rate of falling sodium is higher than that of a sodium stationary pool. This also applies to the other combustion effects. However in order to spray outflowing sodium, definite conditions are needed which are seldom met in practice. Drop combustion effect should be taken into account in the case of outflowing jet spraying caused by some barrier (e.g. floor or walls of the room). Continuous sodium spray does not burn because of time deficiency. As at any fire, smoke is one of the most dangerous factors in the sodium burning process. Sodium fume consists of combustion products which have become aerosols. These aerosols are sodium oxides (Na₂O and Na₂O₂) which enter immediately into interaction with the atmospheric components, steam and carbon dioxide producing sodium hydroxide-alkali (NaOH) and sodium carbonate (Na₂CO₃). The oxide into hydroxide transformation process takes a few seconds after particle formation. Sodium carbonate is formed more slowly. This process velocity depends on atmosphere humidity and takes several minutes. Both chemical and physical transformations of particles take place during the fume propagation as well as conglomerate formation of particles formed in the combustion process. At relative air humidity over 35%, dry particles are transformed into fluid drops. There are changes of their density and size distribution mode. Sodium particle radius lie in wide range: from several hundredths of micron to tens of microns. In the process of aerosol propagation they deposit on the floor, ceiling and walls of the room, as well as on the surfaces of equipment and ventilation piping. This particle precipitation takes place mainly because of gravity: up to 80% of all particles are deposited on the floor. Aerosol particles can cause equipment damage, and this concerns first of all electrical equipment and instrumentation, these particles are also dangerous for people.

2.3.2. Sodium burning and sodium concrete interaction

NPP rooms where sodium system equipment is situated are usually concrete cells. In spite of steel lining on the internal wall surface there is a probability (though rather low) of sodium and concrete interaction in the case of coolant leak. The basic principles of NPP equipment sodium fire protection are presented in [3-6]. Sodium-concrete interaction features and study results of this interaction effects are presented in [7-14, 16-18]. It has been found that the sodium-concrete interface temperature can exceed 800°C. In this situation chemical reaction takes place between concrete components and sodium resulting in concrete destruction under thermal effect (thermal stresses), hydrogen release, CO₂ and other gaseous
reaction products generation conditions. Results of chemical and thermal process study concerning liquid sodium with concrete interaction are to be considered in short below. Chemical processes mechanisms of sodium and concrete interaction have been studied in Hanford Engineering Development Laboratory (HEDL) and in other US research centers [8, 10, 11, 13]. as well, as in laboratories of the UK [14, 16, 17], France [7, 9, 12, 18] etc.

The main results of the experimental studies are as follows:

1. Water release from the concrete takes place caused by its heating, with release rate depending on concrete space and time temperature distribution which is determined by the external heat source. Sodium-water reaction producing hydrogen is highly exothermic. When the water release rate reaches its maximum, sodium-water reaction rate reaches maximum as well. As it has been demonstrated by measurements of hydrogen formation rate (corresponding to the water release rate), 1 to 2.5 kg/m$^2$h water release maximum rate has been achieved in rather short time (after several hours of interaction) with the following sufficient decrease. Mean hydrogen release rate during initial 8 to 10 hour period of sodium-concrete interaction was 0.2 to 0.5 kg/m$^2$h, and then it fall quickly down to zero.

2. Although modes of chemical reaction front penetration into the concrete varied in every test, there were several common features. The maximum penetration rate took place at the early phase (within 5 hours) and this called for both maximum sodium-water reaction heat release rate and chemical reaction of sodium with the concrete components. The concrete cracking and destruction caused by thermal shock could result in sufficient penetration increase. The following phase (5 to 100 hours) is characterized by a slower penetration. Most of tests have been practically completed with unreacted sodium, concrete and water residuals available. Thus, apparently, there is a depth limit for chemical reaction to penetrate into the concrete.

3. There is a temperature threshold value below which reactions between sodium and concrete solid components go rather slowly. This threshold temperature value depends on the concrete composition and it usually lies in 500 to 600°C range. Thus, sodium reactions with the concrete solid components are of low importance for chemical reaction front penetration into the concrete until the concrete temperature reaches its threshold value.

4. Sodium-concrete reaction products have rather large volume and they produce a layer promoting sodium mass separation from pure concrete. Melting temperature and reaction product density for normally hydrated limestone concrete are respectively 540°C to 600°C and 2.46 to 2.64 g/cm$^3$. The reaction products in the upper layer have lower density and melting temperature values. The products of reaction of sodium with previously dehydrated concrete are melting at the temperatures above 800°C and have a density of 2.6 to 2.7 g/cm$^3$.

5. Both liquid sodium and sodium hydroxide NaOH contribute to the concrete destruction. Experiments carried out under laboratory conditions have shown that NaOH-concrete reaction takes place much slower and it is less exothermic as compared to the sodium-concrete reaction.

6. Hydrated concrete tests have revealed some new features. Originally, sodium-water reaction prevailed for hydrated concrete, resulting in NaOH generation. This hydroxide penetrated into the concrete interacting with it and causing reaction products slime appearance. After temperature threshold value achievement, sodium-concrete solid component reaction role began to increase. In 10 to 50 hours the chemical reactions became less intensive with liquid sodium layer remaining over the reaction product layer. After cooling the reaction products formed very hard,
granite-like bulk, containing (for the limestone concrete) very little amount of reduced carbon. For dehydrated concrete the sodium-water reaction rate was rather low. Most important was that the sodium-concrete solid component reaction going on for many hours at constant rate usually till the sodium was completely spent. When cooled products solidified they formed porous bulk containing reduced carbon amount 3 times more compared to the above case. For the hydrated concrete, sodium concentration sufficiently decreases with the reaction product layer depth demonstrating that sodium contact with the concrete is restricted owing to the reaction products slime presence.

7. Limestone and magnetite concretes have revealed more resistance to the sodium attack in comparison with the basalt concrete, thus confirming the importance of chemical effects depending on concrete characteristics.

8. In most cases vertical concrete surfaces have been damaged much more then horizontal ones that reveals the effect of spatial disposition of sodium-concrete interface on the interaction process. Thus, sodium and concrete interaction could be of three different types.

*Sodium-concrete water interaction:*

\[
2 \text{Na} + \text{H}_2\text{O} \rightarrow \text{NaOH} + \frac{1}{2} \text{H}_2
\]

\[
4 \text{Na} + \text{H}_2\text{O} \rightarrow \text{Na}_2\text{O} + [2 \text{NaH} = 2 \text{Na} + \text{H}_2]\]

Today there are no definite data indicating which one of these two reactions prevails. In some publications [13, 14] there is information about large NaOH amount (up to 37%) contained in the reaction products where Na\(_2\text{O}\) is absent. On the other hand, there is a reason to consider that under high temperature conditions typical for interaction process (more than 318°C - hydroxide melting point), (2) type reaction would prevail.

*NaOH-concrete solid components interaction.* Hydroxide formed in the reaction process interacts with the main concrete components:

\[
\text{NaOH} + \frac{1}{2} \text{CaCO}_3 - \frac{1}{2} \text{Na}_2\text{CO}_3 +
\]

\[
+ [\frac{1}{2} \text{Ca(OH)}_2 - \frac{1}{2} \text{CaO} + \frac{1}{2} \text{H}_2\text{O}];
\]

\[
2 \text{NaOH} + \text{SiO}_2 \rightarrow \text{Na}_2\text{SiO}_3 + \text{H}_2\text{O}
\]

*Sodium-concrete solid components interaction.* These reactions become appreciable after temperature increase above the threshold value which lies in the range 475 to 575°C depending on concrete type. For example, in the case of limestone concrete, sodium-CaCO\(_3\) (MgCO\(_3\)) reaction is typically:

\[
4 \text{Na} + 3\text{CaCO}_3 \rightarrow 2 \text{Na}_2\text{CO}_3 + 3 \text{CaO} + \text{C}
\]

Taking into account sodium carbonate decomposition, the reaction expression will be as follows:

\[
4 \text{Na} + \text{CaCO}_3 \rightarrow 2 \text{Na}_2\text{O} + \text{CaO} + \text{C}
\]
Free silicon oxide SiO$_2$, present practically in any type of concrete, results in the following reaction:

$$4 \text{Na} + 3 \text{SiO}_2 \rightarrow 2 \text{Na}_2\text{SiO}_3 + \text{Si}$$

(6)

As for thermal effects resulting from sodium and concrete contacts there are two processes to be noted: concrete mass change and concrete destruction.

Experimental studies have shown that concrete mass decrease is mainly caused by the water removal from it. As it can be seen from Fig. 2.3.2 water evaporation begins at concrete temperature of 100°C. Initially moisture is removed from large pores. When temperature exceeds 200°C, water evaporation in small pores begins. At the same time gel decomposition (release of water combined with the concrete) begins. The concrete mass change (water release) practically ceases at 800°C. Concrete destruction effect during its interaction with sodium results from the internal stresses gradient arising due to two main reasons. First, intensive evaporation leads to pressure increase in the concrete body. Second, different concrete components (cement, granite, limestone, sandstone etc.) have different thermal expansion coefficients (Fig. 2.3.3).

![Temperature diagram for three concrete types on the base of portland cement. Preliminary concrete cure: 20°C, relative humidity 65%, heating rate 3°K.min. 1 - quartzite concrete; 2 - basalt concrete; 3 - limestone concrete](image-url)
2.3.3. Analytical methods for sodium combustion process evaluation

The main processes, important for evaluation of radioactive sodium leak and combustion consequences, are as follows: (a) sodium outflow: breach dimensions, outflowing sodium temperature and pressure, jet continuous section length, jet break-up mode, sodium spreading mode, (b) sodium combustion: sodium drops combustion, spilled sodium surface combustion, (c) ventilation: room tightness, forced or natural ventilation availability, gas leak rates in and out of the room; (d) combustion products propagation through NPP buildings, and (e) combustion products propagation through the environment. There have been a lot of experimental studies in different countries on the development of calculation methods for the sodium combustion parameters evaluation. On the basis of data presented in [20-27] one can propose a generalized approach to this problem solution. This approach is presented below.

A. Breach dimension effect.

All sodium system pipes are coated with thermal insulation. If the piping wall breach is small, this insulation serves as a leak catch: sodium does not escape its limits. Insulation break-through takes place in the case of piping breach equivalent diameter about 10 mm or more. If so, thermal insulation influence could be omitted.
**B. Sodium outflow hydrodynamics.**

**Continuous jet.** As numerous experiments have shown, continuous jet of outflowing sodium does not burn. The jet break-up is caused by either its hydrodynamic instability or its impact against a barrier. Jet continuous section length \( L \) for such low viscous fluid as sodium can be evaluated by the following expression [20]:

\[
L = 8.46 \ U_o \sqrt{\frac{\rho \ a^3}{\sigma}}
\]  

(7)

where \( U_o \) - fluid outflow velocity,
\( \rho, \sigma \) - fluid density and surface tension coefficient respectively,
\( a \) - jet radius.

The following condition is the condition for (7) expression applicability:

\[
\sqrt{\frac{a \ \sigma}{\rho \ \nu^2}} \geq 1
\]

(8)

where \( \nu \) - fluid kinematic viscosity.

This condition is always satisfied for the sodium under the considered conditions. Calculations made by (7) expression show that in the case of fast reactor sodium system, sodium jet continuous section can be as long as several tens of meters.

**C. Drop formation.**

The most severe situation arises if high leak rate sodium jet breaks down completely into drops. This can occur as a result of vertically oriented jet impact against the room ceiling or some other horizontal barrier [21]. Sodium drop sizes are usually in 1 to 10 mm range. Drops of 0.3 to 0.35 cm radius break down into the smaller ones when they fall. Burning drop diameter \( D \) as a function of time can be presented as follows:

\[
D^2 = D_o^2 - K \cdot t
\]

(9)

where \( D_o \) - sodium drop initial diameter value; \( K \) - combustion coefficient; \( t \) - time.

For sodium \( K \) value is in \( 10^{-3} \) to \( 10^{-4} \) cm\(^2\)/s range [22].

**D. Sodium layer formation.**

When a sodium jet strikes against the ceiling sodium spreads over the ceiling surface. Thus a sodium layer is formed with falling drops on its periphery. Experimental studies have shown that the layer covered spot diameter is equal to 0.63 \( U^{1/3} \), where \( U \) has m/s dimensions [23]. In case of a downward jet, the sodium spreads over the floor of the room. In order to evaluate the radius \( R \) of liquid covered spot on the horizontal plane as a function of time one can use the following relationship [24]:

\[
\frac{R}{\sqrt[3]{G \ t}} = 0.46 (g \ G \ t/\nu^2)^{0.08} (g \ t^2 \sqrt{G \ t})^{0.06}
\]

(10)

where \( G \) - leak volume flow rate; \( g \) - gravitation acceleration; \( \nu \) - kinematic viscosity of fluid; \( t \) - time.
E. Combustion thermal effects.

In the case of sodium spreading, a flame zone is formed above its surface. This zone temperature \( T_f \) is determined using heat balance equation:

\[
B_{Na} Q_c = Q_{f,g} + Q_{f,Na},
\]

where \( B_{Na} \) - sodium combustion rate; \( Q_c \) - combustion reaction heat; \( Q_{f,g} \) - heat transfer from flame to gas in the room; \( Q_{f,Na} \) - heat transfer from flame to sodium.

\( Q_{f,g} \) and \( Q_{f,Na} \) heat flows are determined taking into account convection and radiation heat transfer at given \( T_g \) and \( T_{Na} \) temperature values.

Sodium combustion rate \( B_{Na} \) and oxygen flow rate \( B_{O2} \) coming into the flame zone are calculated using the following equations [25]:

\[
B_{Na} = \frac{\rho_{Na} D_{Na}}{8} \left[ \frac{P}{P - P_{Na}} \right],
\]

\[
B_{O2} = \frac{D_{O2} Nu_D}{l} \rho_{O2},
\]

where \( \rho_{Na}, \rho_{O2} \) - vapour densities of sodium and oxygen respectively,
\( P_{Na}, P \) - saturated vapour pressure of sodium and gas pressure respectively,
\( l \) - characteristic horizontal dimension of sodium layer.

Vapour diffusion factors for sodium (\( D_{Na} \)) and (\( D_{O2} \)) can be calculated by the relation:

\[
D = D(0) \frac{P_0}{P} \left[ \frac{T}{T_0} \right]^{1.75},
\]

where \( D(0) \) - diffusion factor value at \( P_0 \) and \( T_0 \).
For example, \( D_{Na}(0) = 0.76 \text{ cm}^2/\text{s} \), \( D_{O2}(0) = 0.86 \text{ cm}^2/\text{s} \) at \( P_0 = 1 \text{ bar} \) and \( T_0 = 700 \text{ K} \).

Diffusion Nusselt number:

\[
Nu_D = 0.14 \left( Gr \ Pr_D \right)^{1/3},
\]

where \( Gr \) - Grashof number, \( Pr_D \) - diffusion Prandtl number, \( B_{Na} \) and \( B_{O2} \) values connection is set by the following stoichiometric relationship:

\[
B_{Na} = 2.88 B_{O2}
\]

Solving simultaneously (11) and (16) equations using heat flow equations and (12) to (15) equations, \( T_f \) value can be found.

F. Gas temperature and pressure.

Gas pressure value \( P \) in the room can be defined using the equation of state as follows:

\[
P = \left( k - 1 \right) \frac{U}{V},
\]

where \( k \) - adiabatic exponent \( (k = C_p/C_v) \); \( U \) - internal energy of gas; \( V \) - room volume.
Differentiating it with respect to time we obtain:

\[
\frac{dP}{dt} = \frac{k - 1}{V} \left[ \frac{dQ}{dt} + i_1 \ G_1 - i_2 \ G_2 \right],
\]

where \(\frac{dQ}{dt}\) - heat transfer rate to the gas minus heat losses to the walls; \(i_1, i_2\) - enthalpy values of inflowing and outflowing gas respectively; \(G_1, G_2\) - mass flow rates of inflowing and outflowing gas respectively (including gas for combustion).

Gas temperature \(T\) can be calculated using the following differential equation:

\[
\frac{dT}{dt} = \frac{T}{P} \ \frac{dP}{dt} - \frac{RT^2}{PV} \ (G_{T_1} - G_{T_2}),
\]

where \(R\) represents the gas constant.

**G. Aerosol mass concentration.**

Change with time of mass concentration \(C\) of sodium combustion product aerosols in a failed room with leakages can be represented by the differential equation as follows:

\[
\frac{dC}{dt} = I - \lambda_\gamma \ C - \lambda_\sigma \ C,
\]

where \(I\) - aerosol source-aerosol generation rate per room volume unit; \(\lambda_\gamma\) - parameter of aerosol carry-over from the room owing to precipitation process; \(\lambda_\sigma\) - parameter of gas aerosol carry-over from the room owing to gas outflow including that caused by ventilation system.

The aerosol source can be expressed as follows:

\[
I = \frac{B \ \kappa \ S_c}{V},
\]

where \(B\) represents the sodium combustion specific rate; \(\kappa\) - sodium combustion product fraction converted into aerosols; \(S_c\) - combustion surface area; \(V\) - room volume.

On the base of studies performed by the IPPE \(\lambda_\gamma\) value can be represented by the empirical relationship:

\[
\lambda_\gamma = [1.7 \cdot 10^{-3} \ \frac{S_f}{V} + 3 \cdot 10^{-5} \ \frac{S_w}{V}] \ C^{0.4},
\]

where \(S_f\) and \(S_w\) are the floor and wall surface areas respectively of the failed room. Value dimensions in (22) equation are gram, meter, second. This expression is correct for \(C > 0.5 \ \text{g/m}^3\), i.e. for particle coagulation conditions. For lower \(C\) values when the coagulation processes role is not so important, \(\lambda_\gamma\) values should be calculated using the following formula, derived from the IPPE experimental data:

\[
\lambda_\gamma = 7.4 \cdot 10^{-4} \ \frac{S_f}{V} + 3.0 \cdot 10^{-3} \ \frac{S_w}{V}
\]
The coefficient of sodium combustion product change into aerosols in principle varies within a wide range, depending on specific combustion conditions. However \( x = 0.2 \) is rather conservative for nuclear station buildings. In the case of sodium drop combustion, aerosol generation rate should be taken equal to combustion rate [27].

2.3.4. Sodium fire-fighting methods and systems

Results of analysis of NPP design approaches concerning sodium leak and fire detection and sodium fire-fighting methods and systems used now are presented below.

A. Sodium leak and fire detection. Sodium leak and combustion detection means should meet the following general requirements: (a) emergency signal should be generated at the earliest phase of the incident process so that operators would have time enough to take measures first of all for restriction of size and elimination of small leak development into the large one; (b) high reliability of systems should be provided, in particular by redundancy; (c) the system should precisely register leak location or at least the room, where the leak has occurred, and (d) the detection system false operation probability should be as low as possible.

A number of sodium leak detection methods are known. Either sodium itself or accompanying effects (e.g. fume and radioactivity) appearance are used as input signals. In the former case, the detection system uses electric circuit parameters change due to the sodium contact with some elements of this circuit (spark-plug, electric heating system element, inductive transducer). Sodium flame, sodium vapour, combustion product aerosols, environmental air (or gas) temperature change are the secondary effects of sodium leaks which can be used for the leak detection. For this purpose various types of transducers have been developed including photocells, flame photometers, ionization detectors, thermometers, radioactivity detectors. It is necessary to note that ionization detectors and radioactivity detectors practically can not be installed in primary sodium system rooms because of high temperature and gamma activity conditions. Thus transducers should be situated outside potentially dangerous rooms and representative gas samples should be delivered to them. There is also a possibility of leak detection by personnel. The experience shows that in some cases sodium leak can be detected visually - at the fume presence. So there are special requirements in operational duty regulations of periodical inspections of accessible rooms. A man can feel the effect of sodium aerosol concentration of several mg/m\(^3\) of air, that corresponds approximately to the sensibility of some types of fume detectors. This can be taken into account as well. In the case of large size sodium leak, failed system operational parameters change. This change can be used as additional leak indication.

B. Sodium fire indication and fire-fighting methods and systems. Like any other safety system of an NPP, fire-fighting systems can be distinguished being passive and active ones. For example, if a potentially dangerous cell is filled with the inert gas, excluding combustion process, then this can be referred as a passive principle application to the cell design. The use of inert gas requires a high degree of cell integrity in order to exclude leakages.

Another sodium fire-fighting method (also passive) is termination of nucleation site by means of structural design features with hydrolock device, which provide sodium fire self extinguishing owing to the oxygen burn up to sufficiently low concentration. Sodium tray design is schematically represented in Fig. 2.3.4. Most part of spilled sodium flow freely down to the tray through the draining pipes. The air is displaced from the tray through little orifice in the upper part. Practically there is no sodium combustion in the tray since an
oxygen safe concentration is soon achieved. The only place where combustion takes place is the tray cover, where 2 to 7% of spilled sodium is burnt. In this case aerosol release is about 25 times less as compared to the open sodium combustion.

Many different types of trays have been considered. Their common shortcoming is their difficulty, connected with post accident sodium residue removal. This shortcoming is excluded in the case of special assembly vessels installed at lower elevation under the rooms where sodium leak can occur. These vessels volume should be sufficient to hold the maximum possible amount of spilled sodium. The floor in the room should have some slope to special chutes in order to provide rapid sodium draining into the vessels. There should be sufficient diameter piping, connecting chutes to the vessels. Vessels are filled with inert gas under pressure near atmospheric value. During normal operation the chutes are sealed with elements having low melting temperature. Sodium drained into the vessels can be utilized after its purification.

Expanding powder compositions are also used as passive sodium fire-fighting means. Powder in briquettes or packed in polyethylene bags is placed over the room floor in the area of possible sodium spillage. When getting in contact with sodium, the extinguishing composition expands giving 70 to 100 times volume increase. The powder comes to the sodium surface separating it from the air oxygen. Owing to this, combustion ceases.
Sodium self extinguishing method applied for sodium system buildings includes both passive and active principles. In the case of large sodium amount spillage and ignition, oxygen content in the room atmosphere decreases owing to its burn up, to the value of 4 to 5 vol.% at which combustion is self-suppressed. To provide this sodium fire self extinguishing, the room should be gas tight, and this is achieved, first by sealing off all penetrations, man-holes, doors etc. during construction and assembly period and, second, by changing from forced ventilation to exhaust ventilation in emergency case. Ventilation circuit change-over is carried out automatically by fire sensor signal, so this method can be attributed to active ones. However fire extinguishing is carried out in passive mode.

Room tightness requirements concerning fire self-extinguishing are not very strict. When the exhaust ventilation is in operation, air ingress through slots into the room should not exceed the room volume per hour at 50 mm of water vacuum related to the pressure value in the rooms where the leakage comes from.

Various powder compositions delivered to the combustion nucleation site either by personnel or by some remote supplying systems are active type sodium fire-fighting means. There are a lot of various fire extinguishing powders; from natural ones (sand, soda, alumina) that were used at the early stage of sodium technology development, to those specially developed and patented in many countries (GRAPHEX, MARCALINA). Powders made on the basis of graphite and alkali metal salts are used for sodium fire-fighting. At the same time compositions based on specially treated vermiculite - hydromica group minerals, are widely used.

Special fire extinguishers, delivered manually or mechanically have been developed as well as remote powder supply systems. These systems consist of reservoirs containing fire extinguishing substance, tubes or hose pipes for substance delivery to combustion nucleation site, and pressure generating devices for forcing substances out of the reservoir. It is necessary to note that powder supply systems are rather complex and not reliable in operation. This restricts their practical application. Inert gases and their mixtures can also be used as active fire-fighting means. However, this is only possible provided some requirements are met. The room should be properly sealed. Powder supply to the room should not result in high positive pressure - this requirement is met by gas discharge from the room during powder supply, so that supply and discharge flow rates balance.

REFERENCES

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[18] Soule N. Analysis of the Sodium Concrete Interactions with the Nabe Code. Specialists Meeting Sodium Fires, 6-9 June, 1988, Obninsk, USSR.
2.4. COOLANTS FOR FAST REACTOR

Analysis of various coolants for fast reactor application has been performed in detail by many authors, including those of [1, 2, 3]. The most important physical, thermal and hydraulic and process requirements to the fast reactor coolant have been defined, the whole spectrum of principally accessible coolants have been analyzed, including steam, helium, mercury, lead, lead-bismuth, sodium, sodium-potassium alloy, lithium etc. Only sodium and sodium-potassium coolants are widely adopted for fast reactors. Although, mercury has been used for a short period ("Clementine" reactor in the USA, and BR-2 reactor in the USSR, having thermal output of 30 kW and 100 kW respectively).

The main characteristics comparison of the most suitable coolants for fast reactor application (steam, helium, sodium) is given below in Table 2.4.1. Lead characteristics have been included into the table additionally since lead use as a coolant for future fast reactors has been under consideration recently [4, 5, 6]. Of course, nowadays different points of view can exist on the possibility of lead coolant application for fast reactors. Nevertheless, high density, corrosion activity, high melting temperature value, discrimination to reactor fuel and other features result in serious problems concerning coolant technology. On the other hand, experimental studies, sodium cooled fast reactor development and operation experience, as well as accident analysis results represented in this review have shown that sodium is the best coolant for fast reactors today.

**TABLE 2.4.1. FAST REACTOR COOLANT COMPARISON**

<table>
<thead>
<tr>
<th>Coolant</th>
<th>Advantages</th>
<th>Shortcomings</th>
</tr>
</thead>
<tbody>
<tr>
<td>Steam</td>
<td>- possibility of direct cycle or at most two-circuit heat transfer system;</td>
<td>- primary circuit high pressure;</td>
</tr>
<tr>
<td></td>
<td>- transparence;</td>
<td>- serious problem of reactor decay heat removal in the case of equipment or piping wall rupture;</td>
</tr>
<tr>
<td></td>
<td>- commercial application level;</td>
<td>- high pumping power;</td>
</tr>
<tr>
<td></td>
<td>- minimum of chemical reactions;</td>
<td>- high temperature difference between fuel element cladding and coolant;</td>
</tr>
<tr>
<td></td>
<td>- liquid state at room temperature;</td>
<td>- corrosion activity with respect to structural materials;</td>
</tr>
<tr>
<td></td>
<td>- insignificant activation of coolant itself.</td>
<td>- difficulties concerning coolant quality maintaining required to decrease as much as possible its corrosive effect;</td>
</tr>
<tr>
<td></td>
<td></td>
<td>- absence of technological experience of reactor heat removal provision;</td>
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<tr>
<td></td>
<td></td>
<td>- low breeding ratio;</td>
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<tr>
<td></td>
<td></td>
<td>- possibility of fission product penetration into the turbine in the case of one circuit system.</td>
</tr>
<tr>
<td>1</td>
<td>2</td>
<td>3</td>
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<tr>
<td>---</td>
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</tr>
<tr>
<td>Helium</td>
<td>- two-circuit system (no intermediate circuit); - no activation; - best neutronic characteristics allowing maximum breeding ratio achievement; - transparence; - minimum void reactivity effect; - good compatibility with structural materials; - inertness; - principal possibility of direct cycle realization (on condition that fuel elements are absolutely tight).</td>
<td>- high system pressure; - high pumping power; - necessity for fuel element cladding developed surface provision (for example, by high degree of roughness); - high temperature difference between fuel element cladding and coolant; - insignificant activation of coolant itself; - chemical interaction with water and air without explosion; - sufficient neutronic characteristics allowing high breeding ratio achievement; - reactor decay heat removal provision simplicity; decay heat removal by passive means (natural circulation); - lead boiling temperature is sufficiently higher than reactor vessel temperature under accident conditions.</td>
</tr>
<tr>
<td>Lead</td>
<td>- insignificant activation of coolant itself; - chemical interaction with water and air without explosion; - sufficient neutronic characteristics allowing high breeding ratio achievement; - reactor decay heat removal provision simplicity; decay heat removal by passive means (natural circulation); - lead boiling temperature is sufficiently higher than reactor vessel temperature under accident conditions.</td>
<td>- high temperature difference between fuel element cladding and coolant; - opacity; - intermediate circuit necessity; - reactor vessel high pressure (several MPa); - high corrosion activity with respect to structural materials at satisfactory coolant impurities content level; - complex systems for satisfactory impurities content level maintenance in coolant; - impossibility of metal fuel application because of its active interaction with lead, i.e. exclusion of high breeding ratio needed for future reactors; - high density, requiring special measures to avoid fuel subassembly floating up and leading to problems, concerning passive safety rods development and to additional problems of reactor and primary system antiseismic design development; - freezing temperature about 333°C resulting in heating system necessity to maintain coolant in liquid phase, necessity of refuelling operations performance at 450°C;</td>
</tr>
</tbody>
</table>
TABLE 2.4.1 (Cont.)

<table>
<thead>
<tr>
<th>1</th>
<th>2</th>
<th>3</th>
</tr>
</thead>
<tbody>
<tr>
<td>Sodium</td>
<td>- best thermal characteristics;</td>
<td>- coolant activation;</td>
</tr>
<tr>
<td></td>
<td>- minimum temperature</td>
<td>- chemical activity with respect to oxygen</td>
</tr>
<tr>
<td></td>
<td>difference between fuel</td>
<td>and water;</td>
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<tr>
<td></td>
<td>element cladding and coolant;</td>
<td>- intermediate circuit necessity;</td>
</tr>
<tr>
<td></td>
<td>- low reactor vessel pressure</td>
<td>- opacity;</td>
</tr>
<tr>
<td></td>
<td>(near atmospheric);</td>
<td>- freezing temperature about 100°C heating</td>
</tr>
<tr>
<td></td>
<td>- satisfactory neutronic</td>
<td>system necessity for liquid phase</td>
</tr>
<tr>
<td></td>
<td>characteristics allowing</td>
<td>maintaining; necessity of refuelling</td>
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<tr>
<td></td>
<td>high breeding ratio;</td>
<td>operations performance at about 200°C.</td>
</tr>
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<td></td>
<td>- good compatibility with</td>
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<td></td>
<td>structural materials up to the high</td>
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<td></td>
<td>temperatures;</td>
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<td></td>
<td>- low pumping power;</td>
<td></td>
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<tr>
<td></td>
<td>- reactor decay heat removal provision</td>
<td></td>
</tr>
<tr>
<td></td>
<td>simplicity, decay heat removal by</td>
<td></td>
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<td></td>
<td>passive means (natural circulation);</td>
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<td></td>
<td>- simplicity of systems needed for</td>
<td></td>
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<td></td>
<td>coolant quality maintenance;</td>
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<tr>
<td></td>
<td>- low density, which does not allow</td>
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<td></td>
<td>for fuel subassembly floating, as a</td>
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<tr>
<td></td>
<td>result, the passive safety rod problem</td>
<td></td>
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<tr>
<td></td>
<td>is easily solved (actuation caused by gravity force);</td>
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<td></td>
<td>- boiling temperature sufficiently higher</td>
<td></td>
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<td></td>
<td>than reactor vessel temperature permissible</td>
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<tr>
<td></td>
<td>limits for emergency situation;</td>
<td></td>
</tr>
<tr>
<td></td>
<td>- large positive experience of research and</td>
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<td></td>
<td>commercial application;</td>
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<tr>
<td></td>
<td>- good compatibility with any fuel compositions;</td>
<td></td>
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<tr>
<td></td>
<td>- caesium, strontium, iodine and tritium</td>
<td></td>
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<tr>
<td></td>
<td>dangerous isotopes retention by sodium;</td>
<td></td>
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<td></td>
<td>- developed annihilation methods of sodium</td>
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<tr>
<td></td>
<td>residues on the equipment as well as combustion</td>
<td></td>
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<td>products removal.</td>
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REFERENCES


CONCLUSIONS

Review of experimental and analytical research related to imbalance between reactor core heat generation and heat removal capacities, sodium-water interactions in steam generators and sodium fires produced the following conclusions:

- Reactors transient experiments have determined conditions to fuel pin failure, associated mechanisms and consequences,
- Experimental and calculational studies on core thermohydraulics under flow/power mismatch show that local fuel pin failure does not lead to rapid pin-to-pin propagation,
- In- and out-of-pile experiments on debris bed cooling provide a large amount of information concerning dryout fluxes, and they show that decay heat in a debris bed which is several inches thick can be effectively removed,
- Extensive development and validation of analytical methods and codes has been performed using experimental data,
- Further investigations are aimed at better understanding the postulated accident physical processes in the reactor core taking place under real reactor conditions that would allow improvement of plant economics without reduction in safety,
- Investigations of accident conditions in alternative fuel cores (metal, carbide, nitride, etc) should be intensified,
- Steam generator design are based on single wall tubes and a pressure relief system; SG operating experience has demonstrated that ingress of steam and water into sodium did not result in severe consequences,
- The diversity of SG design and safety systems point to the need for optimization studies which take into account the losses related to operation and maintenance of SG,
- Prevention, detection and mitigation of sodium leaks, improved resistance of nuclear systems to fires and choice of concrete for minimization of interaction with sodium is still an important direction for safety research.