INTERNATIONAL STANDARD PROBLEM

ISP 14

BEHAVIOUR OF A FUEL BUNDLE SIMULATOR DURING A SPECIFIED HEATUP AND FLOODING PERIOD
(REBEKA EXPERIMENT)

(Results of Post-Test Analyses)

Final Comparison Report

H. KARWAT

Lehrstuhl für Reaktordynamik und Reaktorsicherheit
Technische Universität München

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Abstract

This report summarizes the results of the International Standard Problem exercise ISP-14. Posttest calculations are compared to experimentally obtained results. The report has been prepared by the Institute for Reactordynamics and Reactorsafety of the Technical University Munich under contract with the Gesellschaft für Reaktorsicherheit (GRS) which received funding for the ISP-14 activity from the German Ministry for Research and Technology (BMFT) under Contract Number RS470. The REBEKA-6 experiment has been performed by Kernforschungszentrum Karlsruhe (KfK) within the "Projekt Nukleare Sicherheit" funded by the BMFT. The experiment also served as a basis for the German Standard Problem DSP 7 in which 4 German and 4 International Organisations participated by "blind" pretest predictions submitted within the preset deadlines.

The original aim to predict in an integral manner the transient behaviour of an electrically heated fuel simulator bundle with respect to its local cladding temperatures, strains and time to rupture together with the thermohydraulic boundary conditions was not fully achievable. Without decoupling the prediction of the thermomechanic behaviour from the prediction of the thermohydraulic behaviour the requested high accuracy of local temperature predictions (±10 K) would not have been achievable. The applied codes for mechanical fuel behaviour largely demonstrated their capabilities for pretest predictions, when certain local fluid dynamic parameters are well known to the code user. In the "open" exercise 5 organisations participated with their results of analytical simulation procedures. The "open" exercise of ISP 14 confirmed the difficulties expected with the proper analysis of thermohydraulics of the test, caused by the coupling between pin cooling conditions, rod upper plenum calculations and the feedback to clad deformation and burst simulation.

In view of the general importance of fuel behaviour predictions under loss-of-coolant accident conditions it seems highly desirable to improve the common understanding of the important interacting processes by performing at least one more International Standard Problem Exercise in this field. Several aspects of the proper use of codes and code options have been recognized to deserve further attention by experts seeking more convincing simulation methods.
1. INTRODUCTION

A Standard Problem provides an opportunity to compare measured results of an experiment with the calculated results of analytical simulation models which form the basis of computer programs. In general, Standard Problem calculations should preferably be performed without knowing in advance the experimentally measured parameters ("blind" Standard Problem exercises). The comparison between calculated and experimental results may lead to certain conclusions concerning the technical relevance of calculations obtained from different simulation models by different code users. Furthermore the comparison gives the code user the opportunity to control the selection of code options and the arrangement of input data, in particular of the nodalisation.

This exercise served as the seventh German National Standard Problem (DSP 7). With agreement of the German Ministry of Interior (BMI) and of the Ministry for Research and Technology (BMFT) it was simultaneously offered to the CSNI-OECD community to serve as an open International Standard Problem (ISp 14). Additionally, international participants had the opportunity to participate in the "blind" exercise within the time-schedule proposed for the German National Standard Problem exercise. This report compiles the results of the "open" posttest analyses of the REBEKA 6 experiment. A separate report summarizes the results of the submitted pretest predictions /1/.

The task was based on one of the bundle experiments envisaged within the REBEKA non-nuclear fuel simulator research program of the Kernforschungszentrum Karlsruhe (KfK). Specific goal of this experiment was the study of the transient behaviour of an electrically heated fuel simulator-bundle on basis of predictions for local fuel temperatures, cladding-strain, time to burst and the local strain at the location of the burst. Similar processes may be of relevance for nuclear heated fuel bundles during loss of coolant accidents. The experiment has been chosen considering several important aspects. The REBEKA test-facility is supposed to be one of the best suitable non-nuclear test rigs to simulate real nuclear fuel rods. Thus, it appeared appropriate to take one
of these experiments to serve as a basis for the first Standard Problem in the field of fuel bundle behaviour and associated fluidynamic processes.

The specification for the exercise has been set up during Fall 1982 /2/ and was distributed to national and international experts at the beginning of 1983. A preparatory workshop for the participants in the "blind" exercise took place on 2/23/83. The discussion of the exercise and the specification revealed that the requested prediction of the fluidodynamic transient properties within the REBEKA-bundle may result in relatively large predictive error bands. The expected deviations for the pre-test pre-dicted fuel cladding temperature transients were assumed to be larger than those acceptable for a proper description of the material behaviour of the fuel rod simulators. Therefore the workshop recommended certain limitations concerning the requested "blind" predictions of the fluidynamic part of the exercise /3/. Consequently, appropriate time functions for heat transfer coefficients have also been added to the ISP-14 specification package for the "open" exercise.

The experiment REBEKA 6 has been carried out on 3/24/83. An experimental data report describing the REBEKA-6 test /4/ has been distributed after termination of data locking (necessary to carry out the "blind" predictive exercise) with a letter, dated 25.7.83. A preparatory workshop for the ISP-14 participants was planned to take place at KfK Karlsruhe in Septemember 1983. Due to general lack of interest the workshop was cancelled. Deadline for the submission of posttest analyses was finally set for end of February 1984.

The preliminary comparison report was distributed in September 1984 and a workshop to discuss the submitted analyses has been held on November 8th and 9th at the Gesellschaft für Reaktorsicherheit in Garching (Munich).

2. SUMMARY DESCRIPTION OF THE TASK

Figure 1 shows the overall flow scheme of the REBEKA test facility. The main components are the vessel containing the fuel simulator bundle (2), the steam separator (5), the containment
simulator (4) and the connecting pipework. Of main interest was
the bundle of electrically heated fuel rod-simulators. Figure 2
shows the cross section of the bundle. The fuel rod simulators of
3.9 m heated length have been positioned in a 7 x 7-arrangement
with the indicated numbers of the individual rods. The central
rod TF has been replaced by a 10.75 x 0.7 mm Inconel-pipe which
served as a carrier for fluidthermocouples. Figure 2 also shows
the positioning of the temperature sensors of the entire bundle.
All elevations are given in millimeters starting from the top of the
heated section, the 3900 mm elevation being the lower end of the
heated rods.
Figures 3 and 4 show some design details of the fuel rod
simulators. Each simulator consists of an external
Zircaloy-4-cladding, an alumina-oxide annular pellet, an
Inconel-made heater-rod-shield, a boron-nitrit-insulator, the
electrical Inconel heater and a central magnesium-oxide-filler. The
alumina-annular-pellet together with the Inconel-heater-rod-shields
and the external Zircaloy-4 cladding are separated by a 0.05 mm
gap filled with helium (gas annulus). More details about the
design of the fuel rod simulators can be found within the
specification and other relevant KfK-reports /5-8/.

The free gas volume within each fuel rod simulator can be split
up into the following partial gas volumina:

<table>
<thead>
<tr>
<th></th>
<th>20 °C</th>
<th>600 °C</th>
</tr>
</thead>
<tbody>
<tr>
<td>upper plenum</td>
<td>8.5 ccm</td>
<td>8.5 ccm</td>
</tr>
<tr>
<td>gap volume within the heated section</td>
<td>10.0 ccm</td>
<td>10.0 ccm</td>
</tr>
<tr>
<td>3.5 + 5.7 + 0.55 = 10</td>
<td></td>
<td></td>
</tr>
<tr>
<td>lower plenum</td>
<td>17.5 ccm</td>
<td>16.3 ccm</td>
</tr>
<tr>
<td>volume of capillary tubes connecting</td>
<td>4.0 ccm</td>
<td>4.0 ccm</td>
</tr>
<tr>
<td>to the pressure sensors</td>
<td></td>
<td></td>
</tr>
<tr>
<td>total volume</td>
<td>40.0 ccm</td>
<td>39.0 ccm</td>
</tr>
</tbody>
</table>
The nominal change in the partial volume "lower plenum" results from the design of the fuel simulator. The heater rod is fixed within the upper plenum region and is allowed to expand into the lower plenum resulting in the indicated difference of the partial volume "lower plenum" when the temperature is increased from 20 °C to 600 °C. A spring which compresses the annular alumina-oxide-pellets is located within the upper gas plenum. This volume balance does not include the change in volume caused by local straining of the cladding material due to an increased material temperature. All rods except 14 and 54 were pressurized with helium at 60 bar before starting the experiment.

The net flow cross section within the bundle is 57.54 cm². The spacer material thickness is given as 0.42 mm, and thus the spacer cross section of 6.72 cm² yields an 11.5 % relative blockage of the flow channel. The springs connected to the spacer are not considered to cause additional flow blockage.

A simplified flow scheme of the test rig is shown in figure 5. A certain portion of the vapour produced within a boiler (9) flows through valve 7.2 and through the test section (1) to a water collecting tank (2) and finally via a steam transformer (4) to the steam condensator (5). Another portion of the produced vapour flows directly through the valve 7.2 to the steam transformer (4) and to the steam condensator (5). Flooding water flows from the preheater (8) into the lower plenum of the bundle container (1). Before starting the flooding period the flooding water is permanently flowing through valve 7.3 together with some condensing vapour into the main condensator (5). The amount of vapour which flows through the bundle test section during the heatup-period has to be determined by a mass balance. During this period the bundle is heated up by electric power until a certain predetermined cladding temperature is reached at certain preselected rod positions. After reaching these temperature set points the flooding of the bundle is started by closing valves 7.2 and 7.3 forcing the entire coolant water to flood the bundle with a preselected flooding rate. During the flooding period the bundle is heated by 6.6 kW/rod electric power. This amount corresponds to 5 % of full power of a nuclear fuel rod with a radial peaking
factor $f_q = 2.0$ normally not exceeded in light water cooled power reactors. The electric power to all fuel rods was held constant during the flooding period.

The fuel rod simulators have a cosine axial power distribution which results from the design of the internal Inconel-heater-rod (see fig. 6). The total power of 6.6 KW/rod corresponds to the integral of the axial power shape of the rods shown in fig. 7.

3. EXPERIMENTAL CONDITIONS

The Standard Problem experiment REBEKA-6 was carried out on 3/24/83. During the heatup-period the bundle was cooled by steam entering the test section with 4 bar and 150 °C, flowing from the bottom to the top. A mass flow rate of 14 g/s was specified. The experimentalists communicated two different flow rates actually achieved during the test. Earlier, in a letter /9/ dated 3/31/83 the most likely flow rate was told to be 5 g/s, a rate which was confirmed by telephone conversation. The experimental data report for REBEKA-6 /4/ indicates a value of 11.5 g/s to be actually reached during the experiment. According to KfK, the discrepancies between both values originated from an automatic calculation performed within the plot procedures which contained an interpretation error. A mass-balance takes into account partial condensation of steam entering the lower plenum during the heatup-period.

Fig. 1 gives the position of the mass flow measurement devices $D_1$, $D_2$ and $D_3$. The error within the plotting procedure became evident only on occasion of experiment REBEKA-6, which was the first experiment during which the bundle was cooled by an upward steam flow. The 11.5 g/s steam flow rate (steam flow density 0.2 g/cm²s) can be considered as the relevant information. After the cladding temperatures reached the preset value of 765 °C at the axial elevation 1850 mm flooding of the bundle was initiated by closing valves 7.2 and 7.3. This resulted in a flooding rate of 173 g/s (3 g/cm²s), which had been measured at the flow orifice $D_6$ (fig. 1). This value represents the coolant flow rate at the fuel bundle entrance. The flooding
water was preheated to 130 °C, simulating the heatup of emergency core cooling water within a prototype reactor by energy released from hot structures and by heat exchange processes between the emergency core coolant and hot steam leaving the system. Electric power to the bundle and the flooding flow rates were held constant during this period of the experiment until the upper portions of the fuel simulators showed quenching.

As mentioned above information about applicable heat transfer coefficients was communicated to all participants. Rods 14 and 54 have been selected to serve for the determination of these heat transfer coefficients. Both rods were only moderately pressurized with helium up to 5 bar internal pressure to prevent deformation of these rods during the experiment. Temperature measurements, in particular for rod 14 have been processed by the KFK computer program EVA. Measured cladding temperatures and local specific rod power were taken as input to analyse heat transfer coefficients. The fluid temperature was assumed to be constant with 143 °C corresponding to the saturation temperature of steam at a pressure of 4 bar. The method applied in EVA was similar to a procedure utilized earlier within the computer program HETRAP /10/. The reference fluid temperature was held constant for all rod elevations and for the entire duration of the experiment (heatup- and flooding phase). In so far the calculated heat transfer coefficients are artificial, because any local and timewise variation of the real fluid temperature at the locations of interest has been neglected. However, it was hoped that these artificial heat transfer coefficients in combination with the constant reference fluid temperature should warrant an identical treatment of the fluiddynamic boundary conditions by all participants to the "blind" prognostic exercise.

Several participants tried to assess local heat transfer conditions within the bundle without using the heat transfer coefficients submitted to the participants additional to the specification. Some difficulties arose with the definition and determination of the flooding rate. Within Annex I, KFK has provided some more information concerning the applied procedures before and after the test to determine the "cold flooding rate".
4. MEASUREMENT TECHNIQUES

A large number of thermocouples were considered to be essential in understanding this experiment. The instrumentation of the bundle is schematically shown in fig. 2. Many spot welded thermocouples with 0.5 mm diameter have been located in the vicinity of the axial elevation 1850 mm. The rods 49, 20, 29 and 14 were additionally instrumented with thermocouples distributed along the entire length of the rods. Other rods were instrumented with thermocouples concentrated in the vicinity of the expected location of bursts. Some rods were provided with thermocouples located within the fuel rod simulators to study the internal heat transport process. Fig. 2 also indicates the azimuthal location of the some installed thermocouples.

The fluid temperatures were intended to be measured in the vicinity of the spacers 1, 4, 5 and 8 where thermocouples were positioned 15 mm above and 5 mm below the spacers. Additionally, the central Inconel-tube TF was provided with thermocouples of 0.25 mm diameter to measure the axial distribution of fluid temperatures. Unfortunately a large number of fluid thermocouples, mounted on the central Inconel-pipe (rod TF) failed during the experiment.

According to KfK-information the maximum expected error band of the applied thermocouples was assessed to be smaller than 0.3 % related to the measured values. This corresponds to a possible error of ± 3 °C at 800 °C. This error band however does not take into account possible deviations between the measured temperature and the real temperature caused by the external position of the spotwelded thermocouples. Corresponding studies e.g. have been carried out at the Idaho National Engineering Laboratory (INEL) in connection with the interpretation of temperature measurements obtained during loss of coolant experiments at the LOFT reactor, in particular for the blowdown period. The systematic study of these effects showed qualitative influences (delay of quenching) and also quantitative effects (e.g. an improvement of the heat transfer at the location of the thermocouple yielded approx. 50 °C lower temperatures than
uninstrumented rods) which of course are not directly applicable to the REBEKA experiments (see /11/). Similar experiments have been performed by KfK in connection with the REBEKA test program. Larger deviations between internally and externally positioned thermocouples were only observed in case of increased flooding rates causing better cooling of the cladding /12/. According to KfK these effects can be neglected during the REBEKA-6 experiment for the first 50 s after initiating flooding. This is acceptable in particular, as a correlation between the observed local strain and burst processes and the measured temperatures is only possible to a limited extent for bundle experiments.

The internal pressure of the fuel simulators were measured applying pressure sensors obtained from the Gould Inc. Company (Texas-USA). They have been connected to the fuel simulators by capillary-tubes. The error band of these pressure sensors has been assessed by KfK to be in the range of ± 0.5 % of the indicated measured value (calibrated between 50 bar and 70 bar). Proper positioning of the sensors allowed to exclude temperature drifts of the sensors. Certain hysteresis effects were expected to cause somewhat larger errors at low pressures after depressurization of the rods. However, this is considered to be without any relevance for the assessment of this experiment.

System pressure and local pressure differences at the orifices have also been measured by Gould pressure sensors. System pressure error bands were expected to be not larger than 0.1 % related to the measured system pressure and ± 0.25 % related to the measured local pressure differences at flow orifices. These error bands again are of minor importance for the interpretation of the results of this Standard Problem exercise.

5. SUBMITTED CALCULATIONS

Within Table 1 the institutions are compiled which participated in the "open" Posttest-Standard Problem exercise and submitted their
results. It includes information about the involved experts, the applied computer programs and the identifiers of the participants (within the comparative plots the results of participants K to P are distinguished).

More information about specific characteristics of the applied computation and about the rods which have been the subject of the analyses can be found in table 2. One participant (N) had to perform specific considerations to simulate the properties of the electrically heated fuel simulator. Such considerations are normally not necessary if nuclear fuel rods are described by the program. Several participants (K, L and M) did not apply the communicated heat transfer coefficients. They have performed additional studies with respect to the cooling conditions of the analyzed fuel rods. Most noteworthy, participant L reported about his attempt to describe the fluidodynamic conditions of the bundle with the advanced nonequilibrium code TRAC-PD2 /14/. However, a considerable modification of the reflow rate (231 g/s instead of the measured 173 g/s) was required and correction factors for calculated heat transfer coefficients (to allow for assumed grid effects after start of reflood) had to be applied to meet the measured local temperature transients.

A quite different approach was made by participants M who took the measured temperatures of rods 20, 29 and 49 as communicated within the experimental data report as input data for their posttest analysis. Nevertheless, the calculated temperatures for rod 49 exhibit large discrepancies to the measured values.

Finally, some information is added where more detailed descriptions about the computation models can be found.

Table 3 presents some information concerning the nodalisation of the fuel simulators with respect to the axial and radial discretisation. Additionally, those participants are indicated who took into account possible eccentricity between cladding material and the pellets.
Although, several participants initially gave only little information about the applied simulation concept, it became evident that considerable differences exist in the application of analytical methods. This can be seen in particular from the selected nodalisation schemes which are listed for the fuel rod simulation only. Because of a possible general interest, both the axial and subchannel nodalisation for the TRAC-PD2 simulation of participant L have been included and are shown in figs. 8 and 9.

6. COMPARISON BETWEEN CALCULATIONS AND EXPERIMENT

The specification requested the analysis of a number of fuel parameters for the rods 20, 29 and 49 and the associated fluiddynamic conditions. Within the frame of this comparison report not all individual comparative plots for the results of each participant should be included. Preference is given to common comparative plots of all those parameters which have a direct or indirect relation to the analysed material behaviour transients.

It is evident from table 2 that all participants have analyzed the behaviour of the well instrumented rod 49 with respect to the expected temperature transient. Only the participants K and M submitted additional calculations for the rods 20 and/or 29. The main reason for this is the fact that the applied computer models normally do not allow to simulate fluiddynamic subchannel behaviour and hence differences of the cooling conditions of individual rods. Although participants L have performed a fluiddynamic subchannel analysis, they did not attempt to distinguish between the cooling conditions of individual rods.

Therefore, the comparison between analyses and experiment has to focus on the calculated behaviour of the fuel rod simulator 49. Table 4 lists several important parameters calculated for rod 49, in comparison with the measured values from the REBEKA-6 experiment. The table shows that the analyzed time for this rod to burst varies between 104 s and 120 s, compared to the observed rupture time during the experiment at 127 s. This time has been evaluated for the moment at which the measured
transient for the internal pressure exhibits the vertical slope. Participants L were not able to calculate the burst for the average base case rod without modifying the applied creep data of the cladding material.

The final strain at the moment and at the location of the burst has been analyzed by the participants to range between 52 and 104 \% circumferential strain in those cases where burst has been predicted. For participants L who did not predict a burst, the calculated maximum circumferential strain during the experiment is 16 \%. During post-test examination, the circumferential strain at the location of the burst (elevation approx. 1850 mm) was measured to be 55 \%. Obviously, all participants predicted the maximum circumferential strain at the elevation of 1950 mm.

Additionally in table 4 a comparison of the calculated maximum internal pressures and the calculated maximum cladding temperatures for the elevation 1950 mm and the corresponding measured parameters for rod 49 is given. The calculated internal pressure of this rod at the moment of burst has also been assessed, from the transition of the internal pressure of rod 49 into the vertical slope.

The detailed comparison between predicted and measured parameters has to concentrate on the fuel simulator 49. This rod has been analysed by all participants and the results will be discussed in the following section. Figure 10 shows the calculated transient circumferential strain for rod 49, relevant for the location of the burst. The calculations may be compared to the final circumferential strain of 55 \% determined after the experiment. Because it was impossible to measure the circumferential strain as a transient function during the experiment, an experimental time function is not included within fig. 10 (On figure 10 the plot of participant K gives the evolution of the circumferential strain, which is limited at the time of burst to the "average value" before material instability is predicted. This value is supposed to represent the maximum value on each side of the ballooned zone). The final circumferential strain was 55 \%. The axial distribution of the final strain can be seen from
figure 11 which has been extracted from the experimental data report /4/. All participants have submitted the calculated axial profiles of final circumferential strain additionally to the transient information shown for the 1950 mm elevation in figure 10. An attempt has been made to compare calculated strain profiles with the experimental evidence, as can be seen from figure 12.

Figure 13 compares the calculated transients for the internal pressure of rod 49 with the measured time function. Obviously it was not possible to avoid considerable scatter of the calculated transients, although the measured time function was known to the participants.

It seems appropriate to compare the scatter of analyzed pressure-time transients with the measured pressure transients for the internal 18 pressurized fuel simulators which are plotted in figure 14. A relatively close band width of the measured pressure transients during the heatup phase until the initiation of the flooding at 93.5 s may be recognized. After initiation of flooding ballooning starts which finally leads to a rupture of all pressurized rods. Figure 14 does not include the pressure transients of those rods which showed anomalous local behaviour caused by the absence of a pellet /4/. The rather uniform behaviour of the internal pressure of the inner rods may certainly be associated to the lack of meaningful temperature differences between the individual rods before initiation of the flooding. Figure 15 confirms this assumption up to the moment when flooding starts. It shows temperature transients measured at the elevation of 1850 mm for the 24 inner rods (including the unpressurized rods 14 and 54 which served the evaluation of heat transfer coefficients). Equal power release within all rods results in rather uniform temperature transients until the lower bundle level starts to quench. Later on, large differences may be observed for the cladding temperature transients. During the flooding period, this picture corresponds qualitatively to similar temperature margins observed during other reflood experiments (e.g. for the ISP-10 experiment). The cladding heatup rate of all rods was approx. 6.6 K/s at the elevation of 1850 mm until start of flooding, when the heatup rate decreases to values not below
5.0 K/s corresponding to the reduced heating rate. 10-20 s after initiation of flooding turnaround of the temperature curves occurred at these elevations.

Figure 16 compares calculated temperature transients for the elevation of 1850 mm with the measured transient for rod 49 at that location. Figure 17 gives the corresponding curves for the elevation of 1950 mm. For most rods the location of the burst has been found between these two elevations. In addition to the experimental data report /4/ KfK provided a survey picture which indicates circumferential strain at the location of burst and the elevation of each burst for all involved 48 fuel simulators as measured by destructive examination of the bundle after the experiment. This is shown in figure 18 revealing that nearly all rods ruptured between the 1800 mm and 1950 mm elevation (indexed from the top of the heated section). The only exception are rods 15, 18, 20 and 67, which had local pellet deficiencies, and the unpressurized rods 14 and 54.

The calculated temperature transients for rod 49 again exhibit large deviations from the experimentally obtained curve for both elevations during the reflood period. Both figures are indicative for the very limited capabilities of the applied codes to even reanalyze the cooling conditions of a flooding process. This is not at all explainable for those participants who made full use of known measured temperatures or local heat transfer coefficients.

Certain indications about the weaknesses of the analytical temperature distribution models for the fuel rod simulator can be seen from figure 19. For the axial elevation of 1950 mm the measured temperature transient \( T_i \) is compared with the internal temperature transients calculated by the participants. Not a single curve is an agreement with the measured transients! These deviations cannot only be associated to the uncertainties of the coolant conditions at the cladding surface.

Originally, the specification requested the prediction of the coolant temperatures in the vicinity of the assumed location of the
rupture (between 1850 and 1950 mm elevation) and at the coolant channel exit.

The above mentioned compromise to communicate a uniform reference temperature for the fluid (143°C saturation temperature corresponding to 4 bar operating pressure) made this request obsolete for pretest predictions. Some participants to the post-test exercise, submitted the results of own calculations for the coolant channel exit temperature. No result has similarity with the measured transient. This can be easily recognized from figure 20.

For a final assessment of the results of this Standard Problem exercise it is important to compare the main parameters of the REBEKA-6 experiment with other relevant experiments. Within figure 21 this experiment has been correlated with respect to the observed burst-temperature, the burst-pressure and the applied heatup rate before initiation the flooding process. Figure 21 has been extracted from a Status Report describing the main results of studies on the fuel behaviour during loss of coolant accidents obtained within the frame of the "Project Nuclear Safety" at KfK /18/. The behaviour of rod 49 which has been the main subject of the comparison "analyses-experiment" has been added to this figure. The definition of burst-temperature and heatup rate might give some difficulties in the interpretation of this experiment. The heatup rate during this experiment before the beginning of the deformation was approx. 5 to 6 K/s. If we consider both values, the cladding temperature in the vicinity of the burst and the rod pressure at the moment of the actual rupture (transition into the vertical depressurization transient), this experiment apparently will not fit very well into the given picture.

However, we have to take note of the experimental data base which resulted in the REBEKA burst criterion model cited within figure 21. This criterion exclusively has been derived from single rod tests which had been performed under clean fluiddynamic conditions with negligible azimuthal temperature gradients near the location of the burst. Such conditions are in general not warranted for bundle experiments under flooding conditions. Local
cladding temperatures are only known in the vicinity of the location of the rupture. According to the KfK definition, these are not the exact temperatures at the location of the rupture. Furthermore, for bundle experiments KfK experts always expect the formation of local azimuthal temperature gradients, in particular during the flooding phase. Additionally, the rate of local temperature change ("heatup rate") at the moment and at the exact location of the burst can only be roughly estimated from relevant information measured in the vicinity. Thus, according to KfK, the correlation of the burst temperature and observed local burst-strain with the initial "heatup rate" is not correct. Of utmost importance is the temperature gradient during the period of plastic deformation. Nevertheless, the introduction of bundle experiments into figure 21 facilitates a limited comparison. Assessing in detail the measured temperature-time histories in the 1850 mm and 1950 mm elevations during the deformation process we may recognize a variation of the "heat-up rate" from 6.6 K/s at the initiation of flooding down to approx. -4 K/s (cooling rate) at the moment of burst. These facts may help to better position the results of bundle experiments within figure 21, but will not answer all questions concerning the applicability of the KfK-burst criterion.

Figure 22 correlates the observed circumferential burst strain at the location of the rupture to the observed temperature close to the location of the rupture. Again, the rod 49 is positioned within an overview picture similar to that published within the KfK status report. The difficulty to correlate the parameters obtained from most bundle experiments to the REBEKA-burst criterion becomes evident. A detailed analysis of the local cooling conditions at the location of the rupture would be necessary.

Table 5 gives a compilation of the observed circumferential burst strain of some fuel simulators of the REBEKA-6 experiment. Additionally, the cladding temperatures in the vicinity of the rupture and the burst pressure are listed. The scatter of the observed circumferential burst strain can be recognized. Considering only the internal 5 X 5 arrangement of rods and eliminating rods with anomalous behaviour (15, 18, 20 and 67), we
find a variation of the burst strain between 31.7 % and 63.7 %, and a variation of the cladding temperatures at the moment of the rupture between 704 and 814 °C. With respect to the cladding temperatures at the moment of the rupture rod 49 behaves as an average rod, while the observed local circumferential burst strain of rod 49 corresponds to upper values. The pressure at the moment of the rupture varies between 59 bar and 65 bar. With respect to the burst pressure rod 49 can be located at the lower bound. Burst time was only exceeded by one more rod(Nr. 5), while all other rods ruptured earlier.

From comparisons with other bundle experiments we may conclude that the REBEKA-6 experiment reveals no specific anomalies. Nevertheless, the correlation of important experimental parameters to the REBEKA-burst criterion may result in problems. Azimuthal temperature gradients of about 40 K have been observed from temperature measurements of rods 66 and 64 (see fig. 2) during the REBEKA experiment according to KfK. A fuel behaviour code should be able to predict such temperature gradients essential to predict fuel behaviour with reasonable accuracy.

However, the result of the "open" ISP-14 exercise is discouraging with respect to the fluiddynamic analyses of local cooling conditions. We may admit the limited prognostive capabilities of nowadays fluiddynamic codes to prevent reasonable pretest-predictions for the combined fluiddynamic-thermomechanic process of fuel rod behaviour. But the insufficient result of the retrospective analyses submitted within the frame of the "open" exercise should be understood as an unquestionable signal for the large gap existing between the required accuracy of temperature predictions (to enter as boundary condition into the thermomechanic fuel behaviour simulation) and the achievable accuracy of fluiddynamic codes to analyze or predict local cooling conditions of bundle experiments under flooding conditions typical for loss of coolant accidents. Certainly, code users' experience and skillness may also be influential on these findings. Little progress may be recognized if such findings are compared to conclusions drawn from the result of the earlier ISP-10 (Refill and
Reflood Experiment in a Simulated PWR Primary System-PKL) performed in 1981 /19/.

Thus, the experiment should also be correlated to a simple probabilistic burst criterion developed by GRS with respect to the expected circumferential burst strain as function of the burst temperature. This criterion has been developed by applying pure statistical methods to correlate the result of 750 burst experiments without differentiating with respect to the experimental conditions of each experiment /20/. It seems to be easy to compare the results of REBEKA-6 to this criterion. Rod 49 corresponds to the expectation and the scatter of observed results from other the rods corresponds to the usual scatter of the method, as evident from figure 23.

7. DISCUSSION OF THE STANDARD PROBLEM RESULTS

In the frame of this exercise, a first attempt has been made to predict and analyze the behaviour of a bundle of fuel simulators during a heatup and flooding period typical for loss of coolant accident situations by the application of several computer models within the frame of an International Standard Problem exercise. Caused by the temperature increase, the material properties of the Zircaloy cladding distort leading to cladding deformation, local reduction in flow area and finally to a burst of the rods. Local flow channel blockage and/or the number of ruptured fuel rods are of primary interest if the behaviour of fuel rods is to be predicted for light water cooled power reactors within safety analyses. Close coupling exists between the cooling conditions of a fuel bundle and the mechanical behaviour of the individual fuel rods. Within the international frame the REBEKA fuel simulator is considered as a suitable method to simulate real nuclear fuel rods by electrically heated model rods.

On occasion of the workshop, held an November 8th and 9th the results of this Standard Problem exercise was discussed in detail /22/. The workshop was also attended by participants which only submitted pretest-predictions to the "blind" exercise /1/ (e.g. CEC-Ispra and National Nuclear Coorporation NNC - United Kingdom) or which missed the deadline for the submission of their
results to the "open" exercise (University of Pisa - Italy) /23/.
After their results have been submitted to GRS several other
participants performed additional studies and calculations and
presented them also during the workshop. These post-submittal
studies investigated important parameters and the ways in which
they should be treated in the future.
In general, it was noted that the exercise was worthwhile,
especially since it focussed attention to modelling difficulties and
areas requiring more attention of code uses.
Review of the results of the pre-test predictions /1/ in
comparison with the results of the post-test analyses showed
little difference in scatter of transient curves. The two factors
causing this observations are
- the strong effects of the thermal-hydraulic conditions at
  rod surfaces and
- the difficulties in a proper analytical description of the creep
  properties of the cladding material and its influence on the
  formation of local deformation (strain).

The attempt to decouple the thermal-hydraulics of the
experiment from the thermo-mechanic behaviour of the cladding
material by communicating "as-measured" heat transfer
coefficients of two unpressurized rods was unsuccessful. An
interesting point in the discussion of the comparison report was
the experimentally observed scatter in rod to rod temperature data
(see fig.15) within the bundle which corresponds to other
experimental observations obtained with undeformed rods
elsewhere (e.g.ISP 10). This again confirmed the fact that proper
fuel clad deformation calculations require greater accuracy in
transient cladding temperature predictions than is presently
achievable with available thermal-hydraulic code calculations, in
particular for two-phase flow periods of core cooling.

Participant K (France) gave explanations on the code, the
modelling of REBEKA and the assumptions used in the calculation.
The strain curve in the preliminary comparison report (Fig.12)
showed no burst for the French submittal. This should be
corrected to show a burst strain of 95%. An empirical deformation
model is used to extrapolate the strain at the location of burst
from the calculated uniform strain 10 cm above and below the
burst point. A more physical model is now being developed in
France to replace this method which is based on recrystallised
zircalloy data.
Participant L (AEEW - UK) described the thermal-hydraulic
calculations performed with the TRAC-PD2 code. The TRAC
calculations required arbitrary modifications to be more
representative of the REBEKA test results. For instance, it was
necessary to multiply the reflood rate by 1.33 to get a closer
turnover in temperature in going from the adiabatic heating to the
reflood period. Also, it was necessary to delay the heating to
prevent overcalculating the bottom of the core by as much as
100°C. Finally the results of the TRAC calculation had to be
converted to heat transfer coefficients based on equilibrium
conditions etc., for input to MABEL-2D. The creep had then to
be multiplied by a factor of 2.6 to get a proper burst strain. The
burst strain then calculated as 32\(\frac{\%}{\%}\) was within the bounds of the
measured data. Several additional post-test analyses have been
performed for the ISP - 14 exercise, which are addressed in some
more detail within Annex III to this report. Another sensitive
feature of fuel rod deformation modelling is the proper treatment
of the internal gas pressure evolution within the fuel rods.
Participant P (Austria) illustrated the problem and provided
additional information on the method applied within the code
BALO - 2A which can be found in Annex IV.
"Blind" exercise participant E (CEC - ISPRA) communicated the
results of some post-test calculations performed meanwhile. For
completeness these results are inserted within this report as
Annex V.
Participant A (IRE - KfK - Germany) performed additional
sensitivity studies already on occasion of the "blind" exercise.
Several parameters were varied and the effects on the analysed
plastic deformation have been shown. Annex VII summarizes the
results of the study which also involves sensitivity to eccentricity
assumptions (3 additional cases).
Also, "blind" exercise participant H (NNC - United Kingdom) per-
formed additional scoping studies with the BART code for
the reflood calculations in lieu of using the supplied heat transfer
data. In the study performed, eccentricity was modelled as a nonuniform heat source.

Using a 215% strain multiplier gave an average burst strain of 40% but a poor pressure internal prediction. Based on the difficulties in calculating the burst strain and burst time, participant H concluded that the REBEKA 6 clad material was a factor of 2 weaker than the REBEKA 5 clad material. Further studies showed the importance of a good thermal-hydraulic entrainment model and the proper use of the correct heat sink temperature. More details can be found in Annex VII to this report which describes the sensitivity studies performed by participant H.

8. CONCLUSIONS AND RECOMMENDATIONS

The International Standard Problem Activity ISP-14 which was based on the REBEKA - 6 experiment of the Kernforschungszentrum Karlsruhe (KfK) Research Center led to the following conclusions and recommendations which after discussion were adopted by the workshop attendees November 9th, 1984 at GRS - Garching:

1. The activity was unanimously considered as an interesting and useful activity, although the envisaged decoupling of the thermo-hydraulics predictions from the pure fuel behaviour prediction method, which had been recognised not to reflect the complexity of a real reactor simulation, was not completely successful, e.g. problems arose in the calculation of the plenum gas temperatures.

2. Fuel rod behaviour simulation can require an accuracy for temperature transient predictions in the range between 10-15 K with an associated accuracy in the heat transfer history prediction, while the fluid dynamic codes do not appear to have achieved this level of accuracy even for the REBEKA simulated reflood transient.

3. Great care must be taken in comparing bundle experiments (and hence reactor behaviour predictions) with predictions for or to experiments with single rods studied under well defined thermal-hydraulic conditions.

4. Even if the fluid dynamically determined local and overall conditions are known with sufficient reliability, the codes
simulating the material properties and the mechanical behaviour of the fuel rods provided a wide range of prediction of possible fuel clad distortion after a loss-of-coolant accident.

5. A local influence of thermocouples on the local deformation behaviour and the influence of the local deformation behaviour on local clad cooling or quenching has to be taken into account if the experimental observations are to be fully understood.

6. It appears that consideration has to be given to fuel distribution inside the cladding, and special emphasis has to be placed on the cladding rupture criteria in order to compute accurately total deformation at rupture.

In view of the general importance of fuel behaviour predictions under loss-of-coolant accident conditions it seems highly desirable to improve the common understanding of the important interacting processes by performing at least one more International Standard Problem Exercise in this field. Several aspects of the proper use of codes and code options have been recognized to deserve further attention by experts seeking more convincing simulation methods.
References


/9/ Schreiben des KfK (K. Wiehr) vom 31.3.1983 an Prof. Dr. H. Karwat, TU München.


/12/ Ergebnisbericht über Forschungs- und Entwicklungsarbeiten des IRB - 1980; KfK - Bericht 3117 (Februar 1981); S. 16 18.


/17/ D. L. Hagrman: Zirconay Cladding Shape at Failure (BALOON 2); EGG - CDAP - 5379 (July 1981).


/21/ G.Sdouz, Österreichische Modellerweiterungen zum Brennstabversagen am Beispiel des Internationalen Standard Problems Nr. 14; ÖFZS Bericht 4276, vom April 1984

/22/ Summary Record of the Final Comparison Workshop on International Standard Problem Exercise 14, held in Garching (FRG) on 8th and 9th November, 1984; OECD Document SEN/SIN (84) 60; 19.11.84.

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<th>Expert</th>
<th>Computer Programs</th>
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Table 1 Participating Institutions and applied Computer Programs
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<th>Model</th>
<th>Calculation Particularities</th>
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Table 2 Survey of Analysed Rods and Calculation Particularities
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All Times are referred to the Begin of the Heatup-Phase

*) 0 ° = North  Δ = Rod with local Fabrication Anomaly
Fig. 1 REBEKA-6
Overall Flow Scheme of the Test Section with Temperature and Pressure Measurement Positions
FIG. 2  REBEKA-6
INSTRUMENTATION SCHEME OF THE BUNDLE (TEMPERATURE MEASUREMENT POINTS)
### Dimensional Margins of the Fuel Rod Simulator

**Fig. 4**

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<td>0,15</td>
<td>0,025</td>
<td>0,075</td>
<td></td>
<td></td>
<td>0,05</td>
<td>0,15</td>
</tr>
</tbody>
</table>
Fig. 5  REBEKA-6
FLOW SCHEME OF THE DEFORMATION EXPERIMENTS, BUNDLE-TEST
FIG. 6  REBEKA-6
Nominal Dimensions of the Fuel Rod Simulator
FIG. 7

REBEKA-6
POWER PROFILE OF INCONEL HEATER

2.6 mm² by an Inconel heater exterior diameter 3.6 mm

N_max/N_average [%] vs. [mm]

- 2.65 mm²
- 3.62 mm²
- 4.4 mm²
- 4.8 mm²

0 150 500 1000 1950 2900 3400 3750 3900 [mm]
<table>
<thead>
<tr>
<th>CORE TOP</th>
<th>TRAC MODEL LOCATIONS (mm)</th>
<th>THERMOCOUPLE LOCATIONS</th>
<th>MABEL MODEL AXIAL HEIGHT SLAB (mm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>3900</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>10</td>
<td>3750</td>
<td></td>
<td>26</td>
</tr>
<tr>
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<td>3400</td>
<td>3510</td>
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<td>3000</td>
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<td>2000</td>
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<td>1950</td>
<td></td>
</tr>
<tr>
<td>4</td>
<td></td>
<td>1560</td>
<td>8</td>
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<td></td>
<td></td>
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<td>1000</td>
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</tr>
<tr>
<td></td>
<td></td>
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<td></td>
</tr>
</tbody>
</table>

**FIG. 8 AXIAL GEOMETRY AND MODELS FOR REBEKA-6.**

(PARTICIPANT L)
FIG. 9 MABEL-2 NODES AND SUBCHANNELS (PARTICIPANT L)
A. SUBDIVISION OF PELLET AND CLADDING INTO RADIAL, AZIMUTHAL AND AXIAL NODES
B. FUEL ROD SURROUNDED BY 3 OTHER RODS ON SQUARE LATTICE SHOWING SUBCHANNEL AND ROD NUMBERING SYSTEM
FIG. 10: COMPARISON: CALCULATED/MEASURED DEFORMATION ROD 49

LEGEND

□ = PART. K 26
○ = PART. L 61
△ = PART. M 96
+ = PART. N 131
× = PART. P 166
◊ = MEAS. 201

104% (extrapolated)

95% (extrapolated)

Exptl. Deformation (final)

close location of rupture

close location of rupture
Fig. 13  Comparison of Calculated/Measured Int. Pressure in Rod 49

Legend:
- □ = Part. K 33
- ○ = Part. L 68
- △ = Part. M 103
- ⊕ = Part. N 138
- × = Part. P 173
- ♦ = MEAS. 215
Fig. 14 REBEKA-6
Pressure-Time Histories of 18 Pressurized Inner Rods
(Without Rod Having Fabrication Anomalies)
Fig. 15 REBEKA-6

CLADDING TEMPERATURE SEQUENCE OF THE 24 INNER RODS FOR THE 1850 MM ELEVATION
Fig. 16  Comparison of Calculated/Measured Temperature for Rod 49
<table>
<thead>
<tr>
<th>No.</th>
<th>Burst Strain (Max. Value)</th>
<th>Fuel Rod No.</th>
</tr>
</thead>
<tbody>
<tr>
<td>No.37</td>
<td>23.7% 1325mm</td>
<td>No.19</td>
</tr>
<tr>
<td>No.19</td>
<td>33.7% 2005mm</td>
<td>No.16</td>
</tr>
<tr>
<td>No.16</td>
<td>36.0% 1870mm</td>
<td>No.34</td>
</tr>
<tr>
<td>No.34</td>
<td>55.0% 1880.0mm</td>
<td>No.23</td>
</tr>
<tr>
<td>No.23</td>
<td>47.5% 2360mm</td>
<td>No.41</td>
</tr>
<tr>
<td>No.41</td>
<td>44.5% 1875mm</td>
<td>No.7</td>
</tr>
<tr>
<td>No.7</td>
<td>31.5% 1995mm</td>
<td>No.61</td>
</tr>
<tr>
<td>No.61</td>
<td>71.7% 2095mm</td>
<td>No.49</td>
</tr>
<tr>
<td>No.49</td>
<td>54.0% 1835mm</td>
<td>No.18</td>
</tr>
<tr>
<td>No.18</td>
<td>51.6% 1980mm</td>
<td>No.66</td>
</tr>
<tr>
<td>No.66</td>
<td>45.0% 1890mm</td>
<td>No.35</td>
</tr>
<tr>
<td>No.35</td>
<td>37.7% 1920mm</td>
<td>No.22</td>
</tr>
<tr>
<td>No.22</td>
<td>45.0% 1810mm</td>
<td>No.80</td>
</tr>
<tr>
<td>No.80</td>
<td>33.2% 1960mm</td>
<td>No.56</td>
</tr>
<tr>
<td>No.56</td>
<td>31.7% 1955mm</td>
<td>No.10</td>
</tr>
<tr>
<td>No.10</td>
<td>63.7% 1825mm</td>
<td>No.25</td>
</tr>
<tr>
<td>No.25</td>
<td>49.5% 1805mm</td>
<td>No.2</td>
</tr>
<tr>
<td>No.2</td>
<td>32.4% 1885mm</td>
<td>No.20</td>
</tr>
<tr>
<td>No.20</td>
<td>94.5% 2060mm</td>
<td>No.67</td>
</tr>
<tr>
<td>No.67</td>
<td>89.5% 1920mm</td>
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<tr>
<td>No.21</td>
<td>58.4% 1350mm</td>
<td>No.11</td>
</tr>
<tr>
<td>No.11</td>
<td>54.0% 2185mm</td>
<td>No.47</td>
</tr>
<tr>
<td>No.47</td>
<td>43.6% 1875mm</td>
<td>No.1</td>
</tr>
<tr>
<td>No.1</td>
<td>36.2% 1930mm</td>
<td>Fluid Lanze</td>
</tr>
<tr>
<td>No.1</td>
<td></td>
<td></td>
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<tr>
<td>No.5</td>
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<td>No.6</td>
<td>42.0% 1815mm</td>
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<td>No.36</td>
<td>43.6% 1865mm</td>
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<tr>
<td>No.28a</td>
<td>25.8% 1920mm</td>
<td>No.29</td>
</tr>
<tr>
<td>No.29</td>
<td>34.7% 1910mm</td>
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</tr>
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<td>33.2% 1855mm</td>
<td>No.4</td>
</tr>
<tr>
<td>No.4</td>
<td>31.7% 1945mm</td>
<td>No.50</td>
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<td>52.5% 1825mm</td>
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</tr>
<tr>
<td>No.39</td>
<td>43.6% 1865mm</td>
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<tr>
<td>No.14</td>
<td>42.1% 1810mm</td>
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</tr>
<tr>
<td>No.57</td>
<td>34.7% 1900mm</td>
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<td>No.44</td>
<td>36.2% 1785mm</td>
<td>No.17</td>
</tr>
<tr>
<td>No.17</td>
<td>42.1% 1885mm</td>
<td>No.15</td>
</tr>
<tr>
<td>No.15</td>
<td>34.7% 1460mm</td>
<td>No.54</td>
</tr>
<tr>
<td>No.54</td>
<td>42.1% 1810mm</td>
<td>No.30</td>
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<tr>
<td>No.30</td>
<td>42.1% 1810mm</td>
<td>No.14</td>
</tr>
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<td>No.14</td>
<td>34.7% 1900mm</td>
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<td>No.48</td>
<td>22.0% 1740mm</td>
<td>No.8</td>
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<td>No.8</td>
<td>34.7% 1925mm</td>
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</tr>
<tr>
<td>No.59</td>
<td>36.2% 1925mm</td>
<td>No.64</td>
</tr>
<tr>
<td>No.64</td>
<td>31.8% 1955mm</td>
<td>No.26</td>
</tr>
<tr>
<td>No.26</td>
<td>42.1% 1930mm</td>
<td>No.81</td>
</tr>
<tr>
<td>No.81</td>
<td>40.6% 1960mm</td>
<td>No.13</td>
</tr>
<tr>
<td>No.13</td>
<td>46.6% 1990mm</td>
<td></td>
</tr>
</tbody>
</table>

**Fig. 18** REBEKA-6

**BURST STRAIN AND BURST LEVELS**
FIG. 19 COMPARISON: CALC./MEAS. INTERNAL TEMP. ROD 49
Fig. 21  Burst temperature vs. burst pressure of Zircaloy claddings
Fig. 22
REBEKA out-of-pile tests
burst strain vs. burst temperature of Zircaloy claddings
**FIG. 23**

**EXPECTED CIRCUMFERENTIAL STRAIN WITH ± 18 UNCERTAINTY BAND FOR VARIOUS EXPERIMENTS**

- + Curve with all Experiment-Values
- Δ Indirect and direct Heating
- ■ Indirect Heating
- ○ Indirect Heating, In-Pile and Bundles
- □ Indirect Heating, In-Pile
- ○ Direct Heating
- ◇ REBEKA 6 (DSP 7)

**BURST EXTENSION [Å]**

**BURST TEMPERATURE [K]**
Cold flooding rate:

For participants, who didn't use the offered heat transfer coefficient together with the constant fluid temperature the exact value of the steam flow during heat-up and the cold flooding rate during reflood was important. Kevin Routledge found that there was a discrepancy between the mass flow of the flooding water of 173 g/s (3 g/cm²) and the cold flooding rate of 3 cm/s.

The value which was chosen as a dominant information for this experiment was the cold flooding rate. Therefore this value had been checked before and after the experiment.

This happened in the following way:

Before test:

The test rig with the bundle had been heated up to 150 °C by steam at a system pressure of about 4 bar. The calculated mass flow for the flooding water at a temperature of 135 °C was adjusted. Then the steam valve 7.2 and the flooding valve 7.3 had been closed. The water level in the bundle rose and was measured by a Δp-transmitter (N2). The cold flooding rate was determined. Taking into account small deviations in the geometry of the housing (101 x 101 mm inner diameter) of the bundle and/or small leaks in the flooding valve 7.3 (this is a cone valve with a cone to cone sealing) the mass flow of the flooding water was adjusted so that the desired cold flooding rate was 3 cm/s. This preexperiment was repeated several times to prove the reproducibility of the desired value (s. Fig. 1).

A crosscheck for the correct measurement of the Δp-transmitter to determine the cold flooding rate are the minimum and maximum water levels which are given by the axial positions of the connection tubes to the transmitter.
ANNEX II

After test:

At the end of the deformation experiment, when the upper end of the bundle has been quenched the power was shut down. The two phase flow mixture of water with steam bubbles collapsed and the rising water level was measured by the $\Delta p$-transmitter (N2).

The gradient of the rising water confirmed the cold flooding rate of 3 cm/s (see Fig. 2).

Remark:

The cold flooding rate is regarded as the important value (the leading value for the experiment) and therefore a certain discrepancy between the measured mass flow of the flooding water and the cold flooding rate is possible.
FIG. 1/1  REBEKA - 6  Pretest to determine the cold flooding rate
<table>
<thead>
<tr>
<th>Ordinate:</th>
<th>Description</th>
<th>Range</th>
</tr>
</thead>
<tbody>
<tr>
<td>1. water temperature T2.1</td>
<td>0 - 1000 °C</td>
<td></td>
</tr>
<tr>
<td>2. water mass flow D6</td>
<td>0 - 1000 g/s</td>
<td></td>
</tr>
<tr>
<td>3. water level N2</td>
<td>0 - 5000 mm H₂O</td>
<td></td>
</tr>
<tr>
<td>4. system pressure P2.2</td>
<td>0 - 10 bar</td>
<td></td>
</tr>
<tr>
<td>5. electric power L4 (8 rods)</td>
<td>0 - 100 kW</td>
<td></td>
</tr>
</tbody>
</table>

**FIG. I/2 REBEKA-6** Determination of the cold flooding rate at the end of the experiment (power off)
Dr. G. Sdouz in:

Österreichisches Forschungszentrum Seibersdorf Ges. m. b. H.
(vormals Österreichische Studiengesellschaft für Atomenergie Ges. m. b. H.)
Austrian Research Centre Seibersdorf

Lenauagasse 10 · A-1032 WIEN · Austria

Herrn Professor
Dr. H. Karwat
Techn. Universität München
C/o Gesellschaft für Reaktorsicherheit
Forschungsgelände
D-8046 Garching

Institut für Reaktorsicherheit
Stadtbüro Wien
Telefon: (0222) 42 75 11*
Telex: 07 / 5400
Telegramm: fzs seib.
Forschungszentrum Seibersdorf
Telefon: (02254) 80**
Telex: 014 / 353

Bankverbindungen
CA-Bankverein: 26-34343/02
E. ö. Spar-Casse: 012-10122
Österr. Länderbank: 106-100-432

Ihr Zeichen Ihre Nachricht vom Unser Zeichen Sachbearbeiter Telefon (Durchwahl) Datum
RS/sd/s Dr. Sdouz **2220 85 01 15

Betreff: ISP-14 Ergänzung

Sehr geehrter Herr Professor!

Gemäß Annex I des Berichtes SEN/SIN(84)60 möchte ich noch eine Modellbeschreibung der Exzentrizität der Pellets in BALO-2A nachtragen.

Bei weiteren Fragen stehe ich Ihnen jederzeit zur Verfügung.

Hochachtungsvoll

[Signature]

(Dr. G. Sdouz)

1 Beilage
AZIMUTHAL TEMPERATURE VARIATION IN BALO-2A

In the BALO-2A code it is assumed to have eccentrically located pellets in the fuel rod starting from the begining of calculation. The eccentrically located pellets generate an azimuthal power distribution. In addition the axial power profile has a cosine shape. This power distribution results in an axially and azimuthally varying temperature distribution which can be expressed by following equation:

\[ T(\theta, \phi, t) = \bar{T} \cdot t \left( 1 + A \left( 1 + \sin \phi \right) + B \sin \phi \right) \]

In most of the experiments power is generated in the form of a ramp. This is expressed by the product \( \bar{T} \cdot t \) (\( \bar{T} \) in K/s and t in s). The size of the power distribution (A and B) is an input value.

However it is not possible to use time- or temperature-dependent eccentricities.
3 December 1984

Dear Professor Karwat,

UKAEA Calculations for REBEKA-6 (ISPL4)

Please find enclosed an account of one of our sensitivity calculations which we promised to send you for inclusion as an appendix to the ISPL4 Comparison Report.

For this case, the clad strength has been reduced by a factor of 2 and rupture is predicted. Our base case, you will recall, gave a strain of only 16%.

With best regards.

Yours sincerely,

[Signature]

D W SWEET

Enc.

cc Dr I H Gibson
    Dr T J Haste
    Mr R Potter
    Mr J Fell
    Mr I Brittain

An Establishment of the United Kingdom Atomic Energy Authority
APPENDIX: FURTHER UKAEA CALCULATIONS FOR ISP14 USING TRAC PD2/MABEL-2D

In the UKAEA submission to ISP14, the base case quoted in the main text of this report, designated Case 1, used creep constants derived from the data supplied by KFK as input to the Standard Problem. However, cladding rupture was not predicted; the strain at the peak axial position reached only ~16% before deformation was terminated by the fall in cladding temperature as the quench front moved up the bundle.

In order to calculate strains more representative of those found experimentally, two cases were run in which the alpha-phase creep rate was doubled and the heater was constrained to maintain an eccentricity of unity within the cladding. This implies that the heater rod would bow so as to keep in contact with the hot side of the cladding, regardless of any mechanical interaction with neighbouring rods. It was expected that under these conditions rupture strains comparable with the minimum measured would be calculated.

The two additional cases were designed to enable a better comparison to be made between calculated and observed axial strain profiles at realistic strain levels, and similarly to allow a better comparison of coolant temperature both in the ballooning region and at outlet. In Case 2, only the centre rod of the 3 x 3 array was allowed to deform, whereas in Case 3, all rods were allowed to deform at the same rate. In both cases, the outer rods remained centred on their original positions throughout.

The results of cases 2 and 3 were very similar. This similarity was attributed to the absence of calculated bypass, as well as the retained clad-heater contact. Only Case 3 is therefore discussed here. A comparison of temperature, strain and pressure histories between Cases 1 and 3 is presented in Figure A1, while a similar comparison of the axial strain profiles is shown in Figure A2.

In Figure A1, it is seen that with the increased creep rate, rupture is calculated at 130s. This is near the end of the observed range of times, while the rupture strain of 31.3% agrees very well with the minimum observed in the 5 x 5 inner array, i.e. 31.7%. Reference to Figure A2 shows that the strain profile is typical of those observed, except that the strain in the intergrid span above the peak strain position is overpredicted by about a factor of about two, even with an allowance for grid cooling effects. This indicates an underprediction by TRAC of the heat transfer at these axial elevations. Part of the difference may arise because premature quenching at the burst sites is not modelled by the codes. If neighbouring rods had rewetted in this way, the local cooling conditions would have been affected.

The calculations terminated at cladding rupture. The present versions of the programs do not allow predictions to continue beyond this point. Coolant temperatures at the ballooning region and at outlet were not significantly different in the various cases studied.
Fig. III/1: Comparison of TRAC/MABEL-2D Temperature Strain Histories for Cases 1 and 3
Case 1

Profile at end of calculation 300 s into transient

Node 16

Hot Side Straight
45μm Initial Heater Offset

Case 3

Profile at cladding rupture 130s into transient

Rupture (true local maximum strain = 0.88)

Hot Side Straight
Heater Eccentricity 1.0
Alpha-Phase Creep Rate x2

Fig. III/2: Comparison of TRAC/MABEL-2D Strain Profiles for Cases 1 and 3
THE GAP PRESSURE MODEL FOR THE CODE BALO-2A

Gert Sdouz

The computer code BALON2 which has been developed as part of FRAP-T cannot calculate the course of the pressure. Therefore a pressure model has been developed and integrated in the code. The basic features of the model are shown in Fig. 1.

The gas volume is divided into 4 parts. In each part the courses of temperature and volume of the gas are determined. Mean values of temperature and volume are calculated for the whole gas and the change of pressure during a time step is determined using the ideal gas equation:

\[ P_2 = P_1 \cdot \frac{T_2}{T_1} \cdot \frac{V_1}{V_2} \]

The four parts of the gas volume are

1) upper plenum
2) lower plenum
3) central part of the gap volume
4) upper and lower part of the gap volume

The gap is divided into the central part VMT where ballooning occurs and into the upper and lower part of the volumes (VRE). The volumes of the plena are kept constant, the volume of VMT and of VRE are calculated from the difference of the ballooned cladding and the fuel pellets. Based on the axial nodalization VMT is calculated pretty well. VRE is approximated with half of the strain of the boundary.

The temperature of the lower plenum is kept constant on the initial temperature because this part is surrounded by coolant with constant temperature. The final temperature of the upper plenum is estimated to be about 200°C. With the use of the axial power profile the mean power and the temperature in VRE is calculated.
### Pressure Model

<table>
<thead>
<tr>
<th>Volume ($m^3$)</th>
<th>Temperature (K)</th>
</tr>
</thead>
<tbody>
<tr>
<td>$const = 12.5 \times 10^{-6}$</td>
<td>$(CT - 416,) \times 0.1 + 416,$</td>
</tr>
<tr>
<td>$2.323 \times 10^{-4} \times (1 + TCE / 2,)^2 - 2.238 \times 10^{-4}$</td>
<td>$(CT - 416,) \times 0.77 + 416,$</td>
</tr>
<tr>
<td>$0.48\pi \times (\sum RD^2 - 16 \times RHY^2) / 16$</td>
<td>$CT$</td>
</tr>
<tr>
<td>$const = 17.5 \times 10^{-6}$</td>
<td>$416,$</td>
</tr>
</tbody>
</table>

$V2 = VPO + VRE + VMT + VPU$

$TP2 = (TPO \times VPO + TPU \times VPU + TRE \times VRE + CT \times VMT) / V2$
Appendix

ISP 14 Post Test Calculations by RODSWELL

JRC Ispra participated (A.1) in the blind problem exercise. Post-test calculations were also done in the course of an assessment (A.2). These are described briefly below.

For the reference blind calculation, independently derived heat transfer data and Zircaloy creep properties had been used. Clad failure was not predicted but this was shown to be marginal. Assessment showed the main error to be due to false assumptions about the plenum temperature which caused a significant error in the rod pressure.

Post test calculations were done using the measured plenum temperature of rod 49 and heat transfer parameters derived from the measurements on the undeformed rod 14. An empirically derived grid effect on heat transfer was used as before and a full eccentric fuel pellet stack assumed.

Clad failure was predicted at 132 sec. The histories of rod pressure and clad temperature at the hot and cold side of the rod are shown in Fig. A1. The table compares predictions with observations and Fig. A2 shows the axial distribution of hoop strain. It is noted that weak material properties were again assumed implying a residual error equivalent to about 20° on clad temperature. This can only partially be explained by a remaining underprediction of rod pressure. The failure strain predicted is at the lower bound of the observations as is expected for a fully eccentric pellet.

Table A1. Comparison of Post Test Calculations with Observations

<table>
<thead>
<tr>
<th></th>
<th>Fail time (s)</th>
<th>Strain (%)</th>
<th>Peak clad Temp (°C)</th>
<th>Rod Pressure (b) Peak</th>
<th>Rod Pressure (b) Fail.</th>
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</thead>
<tbody>
<tr>
<td>Observed</td>
<td></td>
<td></td>
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<td></td>
</tr>
<tr>
<td>- Rod 49</td>
<td>129</td>
<td>55</td>
<td>807</td>
<td>74</td>
<td>59</td>
</tr>
<tr>
<td>Observed</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>- Other Rods</td>
<td>115-132</td>
<td>32-64</td>
<td>795-825</td>
<td>74-77</td>
<td>59-64</td>
</tr>
<tr>
<td>Calculated</td>
<td>132</td>
<td>30</td>
<td>810</td>
<td>73</td>
<td>56</td>
</tr>
</tbody>
</table>
References


Fig.V/1: New reference calculation results (RODSWELL)
Fig. V/1: New reference calculation results (RODSWELL)
Fig.V/2: Comparison of calculated and measured distribution of Circumferential Strain for REBEKA - 6
Sensitivity study related to plastic deformation of Zircaloy claddings
(Calculations carried out by R. Neyder (KFK) with the code system SSYST-3
for the blind German Standard Problem DSP 7)

Case 1.
Assumptions:

- Case 1 of the calculations was based on the given materials data, the
gamey, the input conditions as initial pressure and temperature and the
power histories. The given heat transfer coefficient histories were related
to a constant fluid temperature independent of time and axial level.

- No temperature history for the upper gas plenum was given. Therefore the
plenum temperature was held constant at 416 K.

- No axial heat transfer coefficient profile between the two inner grid spa-
cers was given. Therefore the heat transfer informations for the node at
the axial midplane (1950 mm) were chosen to be representative for the axial
grid span of 520 mm, with the consequence that this axial length ballooned
homogeneously producing an unrealistic large volume increase.

- The calculation simulated symmetrical ballooning.

Results:

Maximum internal pressure of about 70 bar, maximum circumferential strain of
about 10 %, no burst (see fig. 1).

Case 2
Assumptions:

As case 1, but the upper gas plenum was heated. The surface temperature
calculated for the axial elevation of 500 mm was used as the upper plenum
temperature. This is a reasonable approach.

Results:

Higher maximum internal pressure of about 80 bar, higher strain of 14 %, no burst (see fig. 2).

Case 3
Assumption:

As case 2, but a reduced length for the axial zone of plastic deformation. The central node was divided into two parts of 420 mm and another of 100 mm with a reduced wall thickness (minus 0.020). For both central parts the heat transfer condition of the axial midplane (1950 mm) was assumed. For the 100 mm length the power history of the axial midplane was used.

It is reasonable to include tolerances of the wall thickness in a sensitivity study. The wall thickness deviation accepted by the specification for reactor cladding is \( \leq 0.070 \) mm.

Results:

Higher circumferential strain of 17 %, no burst (see fig. 3).

Case 4
Assumptions:

As case 3, but reduction of the heat transfer coefficient (minus 20 %).

If the heat transfer coefficients calculated from measured cladding temperatures of a bundle experiment are given, it is reasonable in a sensitivity study to vary the heat transfer to smaller values.

In an experiment the thermocouple (TC) position and the position of burst is seldom identical. Even for non deformed claddings axial and azimuthal temperature differences develop on the claddings. The radial temperature gradient between the true wall temperature and the measured temperature at the thermocouple junction and small cooling effects by the fin effect of the TC
(increased heat transfer surface, etc.) may even increase such a temperature difference between the "true burst temperature" and the measured temperature used for the calculation of the heat transfer coefficient.

Therefore the calculated heat transfer coefficient especially during reflood is too high.

Results:

Circumferential strain of 35%; no burst (see fig. 4).

Case 5
Assumptions:

As case 3, but reduction of heat transfer of 30%.

Results:

At the beginning of plastic deformation higher cladding temperature of maximum 18 K, at burst lower temperature of about 26 K, but burst with a circumferential strain of 96% (see fig. 5).

Case 4 and 5 show the dominant influence of the cladding temperature on the plastic deformation of Zircaloy.

Case 6
Assumptions:

As case 3, but instead of the heat transfer coefficient history of rod 14 at axial elevation 1950 mm that of rod 54 at axial elevation of 1850 mm was used.

Results:

About the same temperature, pressure and strain curves (see fig. 6).
Case 7-9

Assumptions:

As case 6, but azimuthal temperature differences were be taken into account. The model AZI was used which calculates non symmetric ballooning behaviour with an eccentricity-model i.e. the gap width between the pellets and the cladding is different on the circumference.

The definition of the eccentricity $e_x$ is:

$$ e_x = \frac{S_2 - S_1}{D - d} $$

$s_2$ = largest gap
$s_1$ = smallest gap
$D$ = inner diameter of the cladding
$d$ = outer diameter of the pellet

$e_x = 0$ (concentric case ($s_1 = s_2$))
$e_x = 1$ (excentric case = gap is closed at one side ($s_1 = 0$))

Results:

Figs. 7-9 show calculations with excentricities of 0.9, 1.0 and 0.8. The burst times were 166 s, 158 s and 184 s.

In all these cases the claddings burst with circumferential strains between 42 and 45 % (see figs. 7-9). In case 8 ($e_x = 1.0$) the cladding temperature on the hot side shows the highest values and on the opposite cold side the lowest values compared to all other cases. The maximum azimuthal temperature difference at burst has been calculated to be 83 K.

The calculated temperature transient for the hot side may be too high, because the heat transfer coefficient was determined with a radial gap size of 0.050 mm, and AZI assumes for the case $e_x = 1$ only a very small gap and heat resistance for this azimuthal node. (The circumference is divided into 36 nodes, i.e. the gap size of the hottest node, which represents 1/36 of the circumference, is an average between gap size equal 0 and a very small value).
ANNEX VI

The maximum cladding temperature is responsible for the burst, therefore the burst time in the calculation may occur earlier, i.e. less time for plastic deformation and underprediction of burst strain. On the other hand the calculation for the temperature transient for the cold side gives a too low value. The reason is the fictive fluid temperature of 416 K which is lower than the real fluid temperature in the experiment. With the increasing gap resistance the calculated cladding temperature drops too fast resulting also in an underprediction of the burst strain.

Therefore R. Meyder decided to favorize the calculation of case 7 with an eccentricity of $e_x = 0.9$.

Case 7 shows a lower maximum temperature, therefore a longer time to burst, compared to case 8 and higher minimum temperature. However, the calculated circumferential burst strain may be underpredicted, because no correction by a reduced heat transfer coefficient has been taken into account in this calculation. (Justification for this argument see cases 4 and 5).

**Final remarks**

This annex discusses which kind of corrections are reasonable and necessary when using the given information for such a "Standard Problem".

Adaptions with such physically reasonable effects make sense and avoids "tuning" by modifying material properties.
Fig. VI/1: Internal overpressure, cladding temperature and circumferential strain at elevation 1950 mm, assumptions case 1.
Fig. VI/2: Internal overpressure, cladding temperature and circumferential strain at elevation 1950 mm, assumptions case 2
Fig. VI/3: Internal overpressure, cladding temperature and circumferential strain at elevation 1950 mm, assumptions case 3
Fig. VI/4: Internal overpressure, cladding temperature and circumferential strain at elevation 1950 mm, assumptions case 4
Fig.VI/5: Internal overpressure, cladding temperature and circumferential strain at elevation 1950 mm, assumptions case 5
Fig. VI/6: Internal overpressure, cladding temperature and circumferential strain at elevation 1850 mm, assumptions case 6
Fig. VI/7: Internal overpressure, circumferential strain, and maximum and minimum temperature on the circumference of the cladding surface at elevation 1850 mm, assumptions case 7
Fig. VI/8: Internal overpressure, circumferential strain, and maximum and minimum temperature on the circumference of the cladding surface at elevation 1850 mm, assumptions case 8.
Fig. VI/9: Internal overpressure, circumferential strain, and maximum and minimum temperature on the circumference of the cladding surface at elevation 1850 mm, assumptions case 9.
INTRODUCTION

NNC undertook an evaluation of German Standard Problem No. 7 (ISP No. 14) completely blind - i.e. no information was available on heat transfer in the rig, as well as on the fuel behaviour, when the calculations were submitted. This calculation was discussed in the pre-test predictions preliminary comparison report (Ref. 1). The purpose of this exercise was to provide a test of the available codes' predictive capability for the interactive heat transfer/clad ballooning problem. The codes employed were BART for the calculation of bundle heat transfer and average rod ballooning behaviour, and TAPSWEL for an evaluation of individual rod behaviour using input provided by BART. Since the blind predictions, further calculations have been undertaken. These calculations were not complete at the time of the submission for the open analysis, although they have now been completed and reported (Ref. 2). These calculations were also reported at the presentation meeting for open participants in Munich on 8th and 9th of November 1984.

BACKGROUND

While a number of aspects of the REBEKA rig behaviour were reasonably calculated in the blind exercise, there were a number of shortcomings identified from the analysis:

(i) while burst strains were well predicted, the burst pressure and temperature were overpredicted and burst times were underpredicted. The blind sensitivity study was better but burst times were overpredicted in this case.

(ii) the quench front advance was significantly underpredicted.

(iii) it was known that bursts would be non-coplanar to some
extent and this would affect the calculated blockage and thus the thermal-hydraulics above the blockage. However, there was no method of estimating in advance the degree of non-coplanarity likely and thus, a coplanar assumption was made in the blind prediction thermal-hydraulics calculation. This resulted in high steam temperatures being predicted at the top of the bundle.

(iv) it was known in advance of the blind prediction that the BART entrainment model overestimated the time to initiate entrainment for tests like REBEKA with low forced reflood. An allowance was made for this in the blind submission by including a sensitivity case that attempted to overcome this problem.

The discrepancy in the burst pressure evaluation was found to be due to the outlet plenum gas temperature. There is a large steam temperature drop at the upper core support plate seen in the data, while the code does not explicitly model this plate. The post test analysis used the measured outlet plenum gas temperature.

BART models a single subchannel. The inlet flow rate to this subchannel was modelled to be equivalent to a bundle value of 156 g/sec. This arose due to uncertainty about exact bundle dimension and conditions (see discussion by K. Wiehrin Appendix I). In the post test analysis, this value was increased to 182 g/sec. It has subsequently been found that the flow rate is, in fact, 163 g/sec (Appendix ), although there will be some uncertainty associated with this value. This residual discrepancy is not considered to have a significant affect on the results below. An error was also found in the code quench front model (too high a gap conductivity was assumed for REBEKA type rods). This was corrected for the post-test analysis. A number of other very minor corrections were made to the data in light of further information that became available from both REBEKA 5 and REBEKA 6.
MODELLING

The thermal hydraulic analysis of a representative subchannel of the bundle was undertaken with the BART code, a non-homogenous, non-equilibrium reflood code. The range of possible burst strains and burst times in the test was calculated with a 2-d thermal conduction code, TAPSWEL. The burst is considered to be due to eccentricity of the heater rod in the cladding which causes a local thinning and rupture due to the resultant temperature difference which builds up around the clad. The eccentricity model assumed in TAPSWEL was a hot side straight model, with lift off of the clad assumed when different values of lift-off strain were reached. The probability of particular values of lift off strain was evaluated from an assessment of REBEKA 5 data. This modelling is identical to that assumed in the blind submission.

An analysis was separately undertaken of the creep of the REBEKA 6 material using measured pressures and clad temperatures away from the burst strain region. This analysis suggests that the material was weaker than implied by the supplied creep data by a factor of 2 (see fig. A1). Results are presented below for both the standard (supplied) creep rate and with the creep rate increased by a factor of 2.

RESULTS

The results of key parameters from the test, burst time, burst pressure, burst temperature and burst strain are shown in Table A1. A selection of parameters are compared with the data in figures A2 to A8. The agreement with data is much closer than in the blind prediction particularly for the the key burst parameters.

The results show the importance of an accurate calculation of time to initiation of entrainment on the burst parameters. For the base blind prediction BART overestimated the time to entrainment initiation and as a result the burst temperature
was overpredicted (by $\sim 60^\circ$C) and the burst time underpredicted (by $\sim 10$ secs). In the blind prediction sensitivity study, where the reflood time was adjusted to try and improve the prediction, the burst temperature prediction was much closer to the data (slight underprediction) but the burst time was overpredicted ($\sim 20$ secs) and the range of burst strains was too low (by $\sim 5 - 15\%$). For the post-test prediction, the key parameters were accurately calculated. It is noted that the entrainment calculations in most codes, even the advanced codes such as TRAC, are generally poor and if an accurate prediction of experiments like REBEKA is required, further developments of these models are required.

Another interesting observation from the analysis was that it is necessary to model the correct sink temperature in the heat transfer calculation (i.e. superheated steam and entrained drops as opposed to a gross saturation temperature) in order to accurately calculate all the burst parameters ($T_{sat}$ results in a delayed burst and, in some cases, many prevent burst occurring).

CONCLUSIONS

The post test analysis shows:-

(1) an acceptably accurate estimate of the key burst parameters was made for the REBEKA 6 test.

(2) there is evidence that the REBEKA 6 clad material is weaker than that for REBEKA 5.

(3) accurate modelling of clad ballooning for this type of experiment requires an appropriate sink temperature (i.e. a non-equilibrium heat transfer model) and a good entrainment model.
REFERENCES


<table>
<thead>
<tr>
<th>Parameter</th>
<th>Data (Central 18 Rods Plus 3 Specimen Rods)</th>
<th>BART/Tapswell (Standard Creep)</th>
<th>BART/Tapswell (Creep * 2)</th>
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<tr>
<td>Burst Time (SEC)</td>
<td>114 - 131.5</td>
<td>'AVE.' Rod - 142</td>
<td>'AVE.' Rod - 128</td>
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<tr>
<td></td>
<td>ROD 20* - 102</td>
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<tr>
<td></td>
<td>ROD 29 - 118</td>
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<td></td>
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<tr>
<td></td>
<td>ROD 49 - 127</td>
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<td></td>
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<td>Burst Pressure (BAR)</td>
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<td>'AVE.' Rod - 60</td>
<td>'AVE.' Rod - 59.4</td>
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<td></td>
<td>ROD 20* - 70</td>
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<tr>
<td></td>
<td>ROD 29 - 61</td>
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<td></td>
</tr>
<tr>
<td></td>
<td>ROD 49 - 60</td>
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<td></td>
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<tr>
<td>Burst Temperature (°C)</td>
<td>?</td>
<td>'AVE.' Rod - 771°C</td>
<td>'AVE.' Rod - 750°C</td>
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<td></td>
<td>ROD 20* - 810°C</td>
<td></td>
<td></td>
</tr>
<tr>
<td></td>
<td>ROD 29 - 760°C</td>
<td></td>
<td></td>
</tr>
<tr>
<td></td>
<td>ROD 49 - 750°C</td>
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<td>Burst Strain</td>
<td>32% - 64%</td>
<td>'AVE.' Rod - 41.8%</td>
<td>'AVE.' Rod - 44.6%</td>
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<td></td>
<td>ROD 20* - 95%</td>
<td>SPREAD - 29 - 48%</td>
<td>SPREAD 31 - 56%</td>
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<tr>
<td></td>
<td>ROD 29 - 35%</td>
<td>(FOR CENTRAL)</td>
<td>(FOR CENTRAL)</td>
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<tr>
<td></td>
<td>ROD 49 - 54%</td>
<td>18 Rods)</td>
<td>18 Rods)</td>
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</table>

* Rod 20 is rogue rod and should be ignored

+ Burst temperatures are estimated
REBEKA 6 SUBCHANNEL FLOW BLOCKAGE FOR INNER 18 RODS

Annex VII
FIG. No. A6
BART SUBCHANNEL FLOW BLOCKAGE
(MODIFIED ENTRAINMENT CREEP X 21)

REBEKA 6 SUBCHANNEL FLOW BLOCKAGE
FOR INNER 18 RODS

Annex VII
FIG. No. A7
REBEKA 6 DATA
TAPSWEL RESULTS
(MODIFIED ENTRAINMENT CREEP x 2)

REBEKA 6 BURST STRAIN DISTRIBUTION
December 20, 1984

Dr. H. Karwat
Lehrstuhl Fur Reaktordynamik
Und Reaktorsicherheit
Technische Universitat Munchen
Forschungsgelande 8046 Garching
Federal Republic of Germany

MISUNDERSTANDINGS PRESENT IN THE INTERPRETATION OF THE FRAP-T6 ANALYSIS PERFORMED
BY EG&G, IDAHO - SRW-2-84

Ref: (a) S.R. Wagoner and E.T. Laats, FRAP-T6 Calculations for International
Standard Problem 14, EGG-SAAM-6499

Dear Dr. Karwat:

The purpose of this letter is to correct misunderstandings present in
the interpretation of the FRAP-T6 analysis, performed by EG&G Idaho, at the
Idaho National Engineering Laboratory (INEL) for International Standard problem
14. As discussed in the reference, all results were sent to you using the
reference measurement for the rod as the bottom of the rod. Your reference
measurement was the top of the rod. When all participants results were combined,
our results were interpreted using our reference point as the top of the
rod (i.e. the calculated results were inverted). Consequently, our FRAP-T6
analysis results were overlayed with the data at incorrect rod elevations.
Three figures are enclosed showing analysis results related to your reference
at the top of the rod. All figures are for the rod that was used as a comparison
for all participants, Rod 49.

Figures 1 and 2 show the axial rod temperatures for Rod 49 at nine locations.
Again, the locations shown are now referenced to the top of the rod. The
peak cladding temperature predicted was 815°C, instead of the reported value
of 560°C.

Figure 3 shows the strain calculated for Rod 49 compared with the calculated
results. You will note the close overlay of predicted results with the experimental
results. These changes are also reflected in Table 1 (Table 1 is the same
as Table 4 in your report).
I would also like to reemphasize two approaches used in the analysis. First, these electrically heated rods were modeled with FRAP-T6, as if they were nuclear rods loaded with uranium dioxide fuel. Second, the thermal hydraulic boundary conditions were not well documented in the experiment. Therefore, the surface heat transfer coefficients (HTCs) were provided by the experimentors for the standard problem study. The HTCs were provided for Rod 14, which was not one of the rods in the study. These HTCs were calculated by the experimentors using a constant inlet coolant temperature and the cladding temperatures, as measured by cladding thermocouples. In order to input HTCs into FRAP-T6, coolant temperatures are required. Since these temperatures were not available, a slightly different approach was taken. The cladding temperatures provided for the rods were entered as coolant temperatures and a high HTC ($1 \times 10^6$ W/m²K) was input, thus forcing the cladding temperatures calculated by FRAP-T6 to be the same as the temperatures recorded during the test. The stated objective of studying cladding mechanical response could, therefore, be most accurately fulfilled with the FRAP-T6 code.

If you have any further questions, please contact me.

Sincerely,

S. R. Wagoner
EG&G Idaho, Inc.

Enclosures:
As states

cc: D. Majumdar, DOE-ID
    H. O. Scott, NRC
    J. O. Zane, EG&G Idaho (w/o Enc.)
Fig. VIII-1  Rod 49, cladding temperatures, Nodes 1-6.
Fig. VIII-2  Rod 49, cladding temperatures, Nodes 7-9.
FIG.VIII-3  Rod 49, Circumferential Strain
<table>
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<tr>
<th>Participant</th>
<th>Identifier</th>
<th>Time of Burst (sec)</th>
<th>Circum. Strain at the Time of Burst (%)</th>
<th>Internal Pressure at the Time of Burst (bar)</th>
<th>Max. Cladding Temperature (1950 mm) °C</th>
<th>Max. Internal Pressure (bar)</th>
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<td>CEN-FAR</td>
<td>K</td>
<td>120 (?)</td>
<td>9.5 (?)</td>
<td>69.5 (?)</td>
<td>800</td>
<td>76</td>
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<tr>
<td>AEEW</td>
<td>L</td>
<td>no burst (base case)</td>
<td>16 (%)</td>
<td>-</td>
<td>834</td>
<td>74</td>
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<tr>
<td>EG &amp; G</td>
<td>M</td>
<td>104</td>
<td>59 (%)</td>
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<td>815</td>
<td>81.7</td>
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<td>VTT</td>
<td>N</td>
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