PWR FUEL BEHAVIOUR
IN DESIGN BASIS ACCIDENT
CONDITIONS

THE DEFORMATION, OXIDATION AND EMBRITTLEMENT
OF PWR FUEL CLADDING
IN A LOSS-OF-COOLANT ACCIDENT

A State-of-the-Art Report by the
TASK GROUP ON FUEL BEHAVIOUR OF
CSNI PRINCIPAL WORKING GROUP No 2

DECEMBER 1986

COMMITTEE ON THE SAFETY OF NUCLEAR INSTALLATIONS
OECD NUCLEAR ENERGY AGENCY
38, boulevard Suchet, 75016 Paris, France
THE DEFORMATION, OXIDATION AND EMBRITTLEMENT OF PWR FUEL CLADDING IN A LOSS-OF-COOLANT ACCIDENT

A STATE-OF-THE-ART REPORT

by

P.D. Parsons
E.D. Hindle
C.A. Mann

of the UKAEA
Springfields Nuclear Power Development Laboratories
United Kingdom

prepared for the
Task Group on Fuel Behaviour
of
CSNI Principal Working Group No. 2

NUCLEAR ENERGY AGENCY
ORGANISATION OF ECONOMIC CO-OPERATION AND DEVELOPMENT
The NEA Committee on the Safety of Nuclear Installations (CSNI) is an international committee made up of scientists and engineers who have responsibilities for nuclear safety research and nuclear licensing. The Committee was set up in 1973 to develop and co-ordinate the Nuclear Energy Agency's work in nuclear safety matters, replacing the former Committee on Reactor Safety Technology (CREST) with its more limited scope.

The Committee's purpose is to foster international co-operation in nuclear safety amongst the OECD Member countries. This is done in a number of ways. Full use is made of the traditional methods of co-operation, such as information exchanges, establishment of working groups, and organisation of conferences. Some of these arrangements are of immediate benefit to Member countries, for example by improving the data base available to national regulatory authorities and to the scientific community at large. Other questions may be taken up by the Committee itself with the aim of achieving an international consensus wherever possible. The traditional approach to co-operation is reinforced by the creating of co-operative (international) research projects, such as PISC and LOFT, and by a novel form of collaboration known as the international standard problem exercise, for testing the performance of computer codes, test methods, etc. used in safety assessments. These exercises are now being conducted in most sectors of the nuclear safety programme.

The greater part of the CSNI co-operative programme is concerned with safety technology for water reactors. The principal areas covered are operating experience and the human factor, reactor system response during abnormal transients, various aspects of primary circuit integrity, the phenomenology of radioactive releases in reactor accidents, containment performance, risk assessment, and severe accidents. The Committee also studies the safety of the fuel cycle, conducts periodic surveys of reactor safety research programmes and operates an international mechanism for exchanging reports on nuclear power plant incidents.

The Sub-Committee on Licensing, consisting of the CSNI Delegates who have responsibilities for the licensing of nuclear installations, examines a variety of nuclear regulatory problems and provides a forum for the review of regulatory questions, the aim being to develop consensus positions in specific areas.
FOREWORD

After about a decade of research into the behaviour of fuel in a hypothetical PWR design basis accident the subject was reaching a mature phase during the early 1980s. A large amount of data had been generated by this time and in about 1983 it was considered appropriate to gather the extensive material available into a coherent report addressing the major phenomenological issues and setting out the major conclusions which could be drawn from the experimentation and analysis.

The Principal Working Group 2 on Transients and Breaks, recommended the CSNI to set up a Group to Review the State of the Art on Fuel behaviour and the status of fuel codes.

The CSNI adopted the proposal and a Task Group on fuel behaviour under design basis accident conditions was formed to discuss the preparation of a State of the Art Report. The members of the Task Group are listed at the end of the report.

In April 1982, the UKAEA published a review on "The Deformation of PWR Fuel in a LOCA" as ND-R-701(S) (ISBN-0-85-356149-4). This report was prepared as part of the general extensive documentation being made available for a Public Inquiry to be held in the U.K., on the application by the CEBG to build a SNUPPS type large PWR at Sizewell on the south-east coast of England.

The Task Group considered this document should form the basis of the required State of the Art Report on fuel behaviour under design basis accident conditions.

At the October 1984 meeting of the CSNI Principal Working Group 2 meeting, each member country was invited to submit comments on ND-R-701(S) for consideration by the authors for incorporation into the document and the enlargement of it to form the State of the Art Report.

Many constructive comments were received, and the authors are grateful for the opportunity to include this material into the the State of Art Report and they extend their thanks to all the Task Group members and the researchers in their countries for their valuable comments.

The authors of the State of Art Report are P.D. Parsons, E.D. Hindle and C.A. Mann all from the UKAEA Springfields Nuclear Laboratories, Preston, England.

The report was completed in November 1985.
# CONTENTS

<table>
<thead>
<tr>
<th>Section</th>
<th>Title</th>
<th>Page</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>INTRODUCTION</td>
<td>9</td>
</tr>
<tr>
<td>2.1</td>
<td>LARGE BREACH LOCA</td>
<td>9</td>
</tr>
<tr>
<td>2.2</td>
<td>SMALL BREACH LOCA</td>
<td>10</td>
</tr>
<tr>
<td>3</td>
<td>TYPES OF POSSIBLE DAMAGE TO THE FUEL</td>
<td>10</td>
</tr>
<tr>
<td>4</td>
<td>PRESERVATION OF COOLABLE GEOMETRY</td>
<td>11</td>
</tr>
<tr>
<td>5.1</td>
<td>INTRODUCTION</td>
<td>11</td>
</tr>
<tr>
<td>5.2</td>
<td>THE KINETICS OF THE ZIRCALOY STEAM REACTION</td>
<td>12</td>
</tr>
<tr>
<td>5.2.1</td>
<td>Isothermal oxidation of Zircaloy in steam</td>
<td>13</td>
</tr>
<tr>
<td>5.2.2</td>
<td>The behaviour of alloying elements during Zircaloy oxidation</td>
<td>19</td>
</tr>
<tr>
<td>5.2.4</td>
<td>High temperature oxidation of Zircaloy in impure steam</td>
<td>21</td>
</tr>
<tr>
<td>5.2.6</td>
<td>High temperature oxidation of Zircaloy in high pressure steam</td>
<td>25</td>
</tr>
<tr>
<td>5.2.7</td>
<td>Effect of deformation on Zircaloy oxidation</td>
<td>26</td>
</tr>
<tr>
<td>5.2.8</td>
<td>Stress and dimensional changes in oxidised Zircaloy</td>
<td>27</td>
</tr>
<tr>
<td>5.3</td>
<td>THE EMBRITTLEMENT OF ZIRCALOY FUEL CLADDING</td>
<td>28</td>
</tr>
<tr>
<td>5.3.1</td>
<td>Current embrittlement criteria</td>
<td>29</td>
</tr>
<tr>
<td>5.3.2</td>
<td>Embrittlement and distribution of oxygen in the cladding wall</td>
<td>31</td>
</tr>
<tr>
<td>5.3.3</td>
<td>Embrittlement below thermal shock temperatures</td>
<td>33</td>
</tr>
<tr>
<td>5.3.4</td>
<td>Comparison of embrittlement in-reactor and out-of-reactor</td>
<td>34</td>
</tr>
<tr>
<td>5.4</td>
<td>CALCULATION OF TRANSIENT OXIDATION, HYDROGEN GENERATION AND EMBRITTLEMENT</td>
<td>35</td>
</tr>
<tr>
<td>5.4.1</td>
<td>Calculation of hydrogen generation</td>
<td>35</td>
</tr>
<tr>
<td>5.4.2</td>
<td>Calculation of oxygen distribution</td>
<td>37</td>
</tr>
<tr>
<td>5.5</td>
<td>CONCLUSIONS</td>
<td>39</td>
</tr>
<tr>
<td>6</td>
<td>PLASTIC DEFORMATION OF CLADDING</td>
<td>41</td>
</tr>
<tr>
<td>6.1</td>
<td>THE PROBLEM</td>
<td>41</td>
</tr>
<tr>
<td>6.2</td>
<td>FACTORS CONTROLLING DEFORMATION AND RUPTURE OF CLADDING</td>
<td>41</td>
</tr>
<tr>
<td>6.3</td>
<td>EXPERIMENTAL DATA</td>
<td>43</td>
</tr>
<tr>
<td>6.3.1</td>
<td>In-reactor tests</td>
<td>44</td>
</tr>
<tr>
<td>6.3.1.1</td>
<td>Single rod tests in the FR-2 reactor</td>
<td>44</td>
</tr>
<tr>
<td>6.3.1.2</td>
<td>Single rod tests in the PBFR</td>
<td>45</td>
</tr>
<tr>
<td>6.3.1.3</td>
<td>Single rod tests in the ESSOR reactor</td>
<td>47</td>
</tr>
<tr>
<td>6.3.1.4</td>
<td>Single rod tests in the SILOE reactor, Grenoble</td>
<td>48</td>
</tr>
<tr>
<td>6.3.1.5</td>
<td>Multi-rod tests in the NRU reactor</td>
<td>48</td>
</tr>
<tr>
<td>6.3.1.6</td>
<td>Multi-rod tests in the PHEBUS Loop, Cadarache, France</td>
<td>49</td>
</tr>
<tr>
<td>6.3.1.7</td>
<td>Multi-rod tests in the Halden reactor</td>
<td>50</td>
</tr>
</tbody>
</table>
6.3.2 Out-of-reactor tests .............................................. 50
6.3.2.1 The REBEKA programme at Kernforschungszentrum Karlsruhe (KfK) W Germany .............................................. 50
6.3.2.2 Single and multi-rod testing at KWU, Erlangen W Germany .............................................. 53
6.3.2.3 Single and multi-rod testing at the Japanese Atomic Energy Research Institute (JAERI) .............................................. 56
6.3.2.4 The single and multi-rod burst test programme at Oak Ridge National Laboratory (ORNL) USA .............................................. 58
6.3.2.5 Single and multi-rod testing at Westinghouse Electric Corporation USA .............................................. 62
6.3.2.6 Single rod testing at Argonne National Laboratory (ANL) USA .............................................. 62
6.3.2.7 Single rod testing at Saclay-CEA-France .............................................. 63
6.3.2.8 Single and multiple rod testing in the UK .............................................. 64
6.3.2.9 Measurement of blockage .............................................. 66

7. PREDICTIVE COMPUTER CODES .............................................. 68
  7.1 INTRODUCTION .............................................. 68
  7.2 FRAP-T .............................................. 68
  7.3 SSYST .............................................. 68
  7.4 MABEL .............................................. 69
  7.5 CUPIDON .............................................. 69
  7.6 CARATE .............................................. 70
  7.7 ACCREL-2 .............................................. 70
  7.8 VALIDATION OF CODES .............................................. 70

8. COOLABILITY OF DEFORMED ASSEMBLIES .............................................. 71

9. CREEP OF ZIRCALOY-4 PWR FUEL CLADDING TUBE .............................................. 71

10. DISCUSSION EVALUATION AND CONCLUSIONS .............................................. 73

11. ACKNOWLEDGEMENTS .............................................. 73

12. REFERENCES .............................................. 75

TABLES 1-13 .............................................. 90-101
FIGURES 1-152 .............................................. 102-222
MEMBERS OF TASK GROUP .............................................. 223-224
EXECUTIVE SUMMARY

A considerable worldwide effort has been expended in the experimental study and modelling of PWR fuel behaviour in accident conditions. The substantial amount of information now available allows a mature view to be formulated as to the likely response of fuel to a loss of coolant accident in which the emergency core cooling systems function as designed.

The CSNI requested the authors to prepare a State-of-the-Art Report on the behaviour of PWR fuel in a loss of coolant accident. This State of the Art Report is based on a survey of PWR fuel behaviour in loss of coolant accidents published in April 1982. The Report has been considerably extended and updated to include a substantial amount of recently published information from many countries.

The scope of the report is limited to the oxidation, embrittlement and deformation of PWR fuel in a loss of coolant accident in which the emergency core coolant systems operate in accordance with the design i.e. accidents within the design basis of the plant. The report briefly describes the thermal hydraulic events during large and small breaks of the primary circuit, followed by the correct functioning and remedial action of the emergency core cooling systems and describes the possible damage to the fuel cladding during these events. The basic process of oxidation of Zircaloy-4 fuel cladding by steam and the reaction kinetics of the oxidation are reviewed in detail. Variables having a possible influence on the oxidation kinetics are also considered; steam availability, impurities in the steam, steam pressure, preoxidation of cladding, deformation of cladding, ionising radiation etc. There are a wide range of kinetic and microstructural data available on the oxidation of Zircaloy's at temperatures up to ~1500°C from which the growth of the oxidised layers, zirconia and oxygen stabilised α-phase may be adequately calculated for transients typical of hypothetical design basis accidents. These data also allow the calculation of the hydrogen and exothermic heat generated.

The embrittlement of Zircaloy-4 cladding by oxidation, relative to fracture in the thermal shock of rewetting or by ambient impact forces as a result of post-accident fuel management is also reviewed in detail. Criteria based both on total oxidation and on the detailed distribution of oxygen through the oxidised cladding wall are considered. The embrittlement criteria based on total oxidation, e.g. the 17% equivalent cladding reacted criterion is relatively simple to apply but is shown to be particularly conservative at lower temperatures. The proposed embrittlement criterion based on oxygen distribution in the cladding wall has potential advantages in being independent of temperature and initial wall thickness but is shown to be difficult to apply in practice since it requires an accurate calculation of oxygen distribution across the oxidised clad wall. The calculation of oxygen concentration across the
oxidised cladding wall is complicated by, the transient nature of the cladding temperature during a loss of coolant accident, the effects of hydrogen in steam and mechanical forces on the cladding during a transient. The published computer codes for the calculation of oxygen concentration are reviewed in terms of the model employed and the limitations apparent in these models when calculating oxygen distribution in cladding in the actual conditions of a loss of coolant accident.

The reduction of coolant pressure to a low value results in the internal fuel rod gas pressure producing a tensile stress which may be sufficient to cause plastic distension of the cladding. It could be conceived that the plastic distension, particularly if in the same horizontal plane for a number of fuel rods, impairs the coolability of the fuel. The coolability of the fuel as a result of plastic distension is outside the scope of this report which is concerned only with the behaviour of fuel leading to deformation.

The factors controlling the deformation and rupture of cladding in a loss of coolant accident are reviewed in detail. There is a large volume of literature on the plastic deformation of Zircaloy cladding. Many experiments have been performed ranging from tests on short single rods up to multi-rod assemblies of full length fuel rods in out-of-reactor rigs. Multi-rodded assemblies have also been tested in-reactor. In the range of stresses and temperatures which may be produced in accidents, strains in the range 30%–90% can be produced. It is concluded that co-planar deformation of fuel cladding with diametral strains up to and including those leading to mechanical interaction between fuel rods have been demonstrated experimentally. However, no experiment realistically simulating a design basis loss of coolant accident, that is one with a multi-rod array and simulated reflood cooling, has produced deformation in the cladding which would prevent adequate cooling of the fuel i.e. quenching.

The entire range of conditions which may follow a design basis loss of coolant accident have not yet been explored experimentally. Neither has it yet been shown experimentally what degree of deformation and sub-channel blockage is required to prevent fuel being adequately cooled, but work in progress should resolve these issues satisfactorily.

Data from experimental programmes of clad deformation should continue to be used in the validation of computer codes and models constructed to predict fuel clad deformation in accident conditions. Finally much of the work performed to date on the behaviour of fuel in a loss of coolant accident has been relevant to a large break in the primary circuit. More attention is being, and should continue to be given to, obtaining experimental data and models of fuel behaviour following a small break loss of coolant accident.
1. INTRODUCTION

Although a major loss of coolant from a pressurised water reactor would lead to shut-down by loss of moderator, even if the control rods were not inserted, fission product decay heat generated in the fuel would raise its temperature in the absence of water coolant. The emergency core cooling system is designed to reflood the core, but while this was happening the fuel would undergo a temperature excursion. This might be severe enough to oxidise the fuel cladding, while the combination of high temperature and stress in the cladding might cause it to distend. The fuel cladding could therefore become embrittled, or it might deform and, by closing sub-channels between rods, impede the rise of the ECCS water through the core, making the temperature transient more severe. This paper reviews the studies which have been made of these possible effects.

2. TEMPERATURES REACHED IN TRANSIENT HEATING OF FUEL FOLLOWING A LOCA

A loss of coolant may obviously occur through a breach ranging from a small leak to the largest conceivable fracture of a duct. The latter has been the subject of many studies as a 'design basis accident'. The extreme case of degradation of the core following failure of the ECCS after a LOCA is not treated in this report; reference 1 examines that topic. Below we consider separately only two classes, firstly the large breach and secondly all other sizes of leak under the heading of 'small breach'.

2.1 LARGE BREACH LOCA

For a large breach the blowdown time would be ~ 20 sec, during which time the stored heat from the rods would be removed. The temperature transient which follows is determined by the source of heat in fission product decay in the fuel stack, the transfer of this heat to the cladding, and its transfer from the cladding by the external heat transfer mechanisms. The latter depend in turn on the reflooding sequence of the ECCS. The ECCS water may be injected via the cold leg, so that it refloods the reactor vessel and core from the bottom, or it may be injected via the hot leg, reflooding the core from the top, or both routes of injection may be used simultaneously. The thermohydraulics of reflood will not be addressed here. The 'refill' period before the ECCS water reaches the bottom of the core allows the temperature of the fuel to increase steadily (the 'ramp'). When reflood proper starts the heat transfer is improved as steam is generated and rises carrying entrained water droplets. The rise in temperature is checked and reversed, and finally the temperature of the cladding falls to that of the water as it
is quenched. Deformation and oxidation occur significantly only when the temperature of the cladding exceeds ~650°C, and the studies of cladding behaviour have concerned themselves mainly with the temperature range 600-1200°C, and with times spent in this range of up to ~100 sec.

Experimental data on the behaviour of a full-size reactor core under these conditions are lacking, and in particular the temperatures likely to be attained by the cladding must be calculated using computer codes. Best-estimate calculations performed in this way show temperature-transients not likely to produce significant oxidation or deformation, but when uncertainties are taken into account higher temperatures cannot be ruled out.

2.2 SMALL BREACH LOCAS

This term is used here to cover the spectrum of events where the break in the primary circuit is less than a major one and does not necessarily lead to rapid blowdown and complete uncovering of the core. A loss of coolant is a potential cause of fuel rod deformation when the coolant level drops to uncover the core. If this occurred, the ECCS system would reflood the core after a lapse of time which would be a function of the breach size. Heat transfer for a partially uncovered core is being investigated. (2) Whether distension occurred would also depend on whether the system pressure fell below the internal pressure in the fuel rods; if not, the main effects would be oxidation of the cladding and associated thermal distortion.

The same comment may be made as in the preceding paragraph, that is, that best estimate calculations show the core remaining covered with water, or at least cooled with no undesirable consequences. The spectrum of small breach accidents has however until recently received less attention than the design-based accident large-breach LOCA, even though the latter seems the least probable of the range of LOCA events. Since the Three Mile Island-2 accident, (3) which was a small-breach LOCA, this neglect is being remedied.

3. TYPES OF POSSIBLE DAMAGE TO THE FUEL

Following a LOCA the fuel rods may be oxidised or deformed, or both. The consequences of such damage may be categorised as follows:

1. Oxidation resulting in embrittlement and fracture of the cladding, with the undesirable consequences of:

- Possible loss of coolable geometry
- Release of fuel and of fission products
- Generation of hydrogen
- Generation of exothermic heat

2. Plastic deformation of cladding resulting in the restriction of coolant flow in the sub-channels between rods. Rods may swell so far as to rupture, releasing fuel and fission products. The mechanisms of oxidation and plastic deformation are reviewed in the following sections.
4. **PRESERVATION OF COOLABLE GEOMETRY**

Impaired coolability could result from plastic deformation of the cladding, leading to constriction of the sub-channels between rods, or from fragmentation of the cladding through oxidation and embrittlement. The coolability of a deformed assembly has been and is being studied in the UK\(^4\) the USA\(^5\) and Germany.\(^6\) The topic is not dealt with in detail in this report, but is briefly reviewed in Section 8.

5. **PRESERVATION OF INTEGRITY OF CLADDING DURING OXIDATION AND EMBRITTLEMENT**

5.1 **INTRODUCTION**

Zircaloy is oxidised by steam over a wide range of temperature and in general terms the reaction becomes significant with respect to clad swelling at relatively low temperature \(\sim 700-800^\circ\text{C}\) whilst its effect on the mechanical integrity of the cladding in and beyond the rewetting stage of a LOCA is more significant at higher temperatures e.g. \(> 1000^\circ\text{C}\).

The important consequences of oxidation are changes in the mechanical properties of cladding, the formation and release of gaseous hydrogen and the generation of additional heat since the reaction is exothermic. The former two phenomena have an impact on the consequences of a design basis accident whilst all three phenomena are of primary importance in severe accidents where the large quantity of exothermic heat released at high temperatures may drive the fuel cladding temperature beyond the melting point of Zircaloy.

The oxidation of Zircaloy introduces complex microstructural changes which tend to embrittle the cladding and if the embrittled cladding fragments, the coolability of the core may be seriously impaired. Additionally, the hydrogen formed eventually vents into the containment building and depending on the circumstances of mixing, may form mixtures of hydrogen/air/steam of sufficient hydrogen concentration that deflagration or detonation may occur, both of which pose a threat to the integrity of safety equipment within the containment and to the containment itself. Oxidation of Zircaloy cladding is a potential source of hydrogen in a LOCA, hence as well as the prediction of the degree of embrittlement of cladding, it is important to be able to predict the amount and timing of hydrogen generation in the transient oxidation of cladding.

The reaction of Zircaloy with steam is a function of temperature and of the rates of supply and dissociation of water molecules at the cladding surface. If gas phase diffusion or surface effects are not rate limiting the kinetics of the reaction are defined by diffusion of oxygen anions in the oxide and the kinetics under these conditions are well established up to about \(1500^\circ\text{C}\) and data is available up to the melting point of Zircaloy-4. In conditions where the steam supply is limited and gas-phase or surface effects become rate controlling the kinetics of Zircaloy oxidation are much less well established.

The embrittlement of Zircaloy cladding arises from the formation of inherently brittle phases, \(\text{ZrO}_2\), \(\alpha\)-\(\text{Zr}\[\text{O}\]) and the diffusion of oxygen into the decreasing width of load bearing \(\beta\)-\(\text{Zr}\) fraction of the cladding. The
ability of the cladding to withstand the thermal shock stresses of quenching during rewetting or post-LOCA forces is related to the extent and detailed nature of oxidation during the transient. The post-LOCA forces which need to be taken into account are the hydraulic, seismic, handling and transport forces.

Early assessment of the embrittlement of oxidised cladding related the propensity to fracture on rewetting with the gross oxidation of the cladding. More recently it has been shown that the distribution of oxygen is particularly important in defining cladding embrittlement and models of oxidation based on ideal diffusion have been proposed for the calculation of the distribution of oxygen across the oxidised cladding wall. Several computer codes are available for oxygen distribution calculations and the present limitations of the models are discussed.

5.2 THE KINETICS OF THE ZIRCALOY STEAM REACTION

Zircaloy fuel cladding is reactive in steam and the products of the reaction can be represented in a simple form by an elementary molecular equation,

\[ \text{Zr} + 2\text{H}_2\text{O} \rightarrow \text{ZrO}_2 + 2\text{H}_2 \quad \Delta H = 586 \text{ kJ mol}^{-1} \quad (1) \]

The rate of reaction is dependent on temperature and at temperatures above ~ 950°C in conditions where the availability of steam is unlimited and the integrity of the oxide film is maintained, is generally believed to be controlled by the rate of diffusion of oxygen anions in the anion deficient zirconia film.

The products of the reaction are also dependent on temperature. At temperatures below the α → (α+α) transus, 810°C for Zr-4, the oxidation of Zircaloy results in the formation of a zirconia film and the diffusion of some oxygen into the underlying α-Zircaloy metal.

At higher temperatures, hexagonal close packed α-Zircaloy is unstable and transforms to body centred cubic β-Zircaloy. Hence, Zircaloy oxidised at temperatures above the transformation temperature consists of an outer layer of zirconia, a layer of high oxygen Zircaloy metal which is stabilised in the α-Zircaloy form by the high oxygen content, and beneath this layer, oxygen diffuses into underlying β-Zircaloy metal. The resulting structure is illustrated in Fig. 1 which shows a section of Zircaloy-2 fuel tube oxidised isothermally in steam at 1400°C for 200 s.

Diffusion controlled solid state processes are characterised by a parabolic rate law. If \( w \) represents the amount of reactant used or product formed then,

\[ \frac{dw}{dt} = \frac{1}{2} \frac{\delta^2}{w} \quad \text{... (2)} \]

The constant of proportionality is the parabolic rate constant and is generally given the nomenclature \( \delta^2/2 \) such that on integration, equation (2) becomes

\[ w^2 = \delta^2 t + c \quad \text{... (3)} \]
where \( t \) is the time of oxidation and \( c \) is assumed to be zero (other systems of notation define the parabolic rate constant \( K_p \), as the constant of proportionality in equation (3) such that \( \omega^2 = K_p \ t \).

The parabolic rate of reaction constant, \( \frac{8^2}{2} \) or \( K_p \), is dependent on temperature according to an Arrhenius expression.

\[
\frac{8^2}{2} \text{ or } K_p = A \exp \left(-\frac{Q}{RT}\right) \\
A = \text{pre-exponential factor} \quad Q = \text{activation energy J mol}^{-1} \\
T = \text{temperature K} \quad R = \text{gas constant } 8.314 \text{ J mole}^{-1} \text{ K}^{-1}
\]

Typical units for the parabolic rate constant are \( g^2 O_2 \text{ cm}^{-4} \text{s}^{-1} \) if the reaction is monitored by total oxygen uptake, i.e. weight gain, or \( g^2 H_2 \text{ cm}^{-4} \text{s}^{-1} \) if hydrogen production is monitored. The growth of the oxide and stabilised \( \alpha \)-Zircaloy layers are also found to obey a parabolic rate law over a wide range of temperature and parabolic rate constants have been measured for the growth of these layers.

Other rates of reaction have also been observed in Zircaloy steam reactions. At lower temperatures e.g. below \( \sim 950^\circ \text{C} \) the formation of zirconia is observed to conform to a cubic rate law whilst stabilised \( \alpha-Zr(\alpha) \) continues to grow according to a parabolic rate law. Additionally at generally lower temperatures i.e. \( < 1050^\circ \text{C} \) and longer times the Zircaloy-steam oxidation reaction transforms to a linear rate i.e. breakaway oxidation is observed.

5.2.1 Isothermal oxidation of Zircaloy in steam

Since the 1950s many measurements of the rate of reaction of Zircaloy in steam have been made and in 1973 the US NRC adopted the isothermal parabolic rate constants published by Baker and Just(7) in 1962 for calculation of oxygen uptake in cladding exposed to steam in thermal transients. Since the NRC adopted the Baker-Just data for regulatory purposes more detailed measurements have been made in laboratories in the US, UK, Japan, W.Germany and Canada. A review of all the available data in 1977(8) showed the Baker-Just data to be conservative and the recently completed programmes of oxidation measurements confirm that the Baker-Just correlation overpredicts the reaction rate.

In the temperature range of interest in LWR conservative accident analyses, the Zircaloy is highly reactive with steam and the high temperatures and exothermicity of the reaction introduce considerable experimental difficulties in determining the kinetics of the reaction. The major sets of data are, however, in general agreement although there are many differences in detail and in the following section the main conclusions from the recent data are briefly reviewed.

The reaction rate constants for Zircaloy-4 in steam were measured in the temperature range 871-1482\(^{\circ}\)C by Biederman et al.(9) Steam had access to both o.d. and i.d. of a Zircaloy fuel tube which was resistance heated in a Gleeble testing machine. The extent of reaction was measured by weight gain and by metallographic measurements of oxidised phase thickness. No effect of steam superheat temperature or flow rate was detected unless the steam
flow rate dropped to a value such that steam availability was limited or hydrogen was not removed efficiently. A tin-rich phase which became coarser with increasing oxidation time and temperature was observed to be precipitated near the middle of the oxide film. Other workers have also observed this phenomena and Biederman suggested that the phase was responsible for the loss of uniformity of the oxide/α-phase boundary at lower temperatures due to the pinning action of the tin-rich precipitates. Above 1482°C the oxide phase also contains stabilised α-phase and a fine unidentified lenticular phase.

The reaction was found to follow a parabolic rate law above the α/β transus and the temperature dependence of the parabolic rate constant for oxygen uptake is shown in Fig. 2 and listed in Table 1. The temperature dependences of the growth of the oxide and combined oxide and alpha phases are listed in Table 2.

The Zircaloy-steam reaction was also investigated by Biederman et al.\(^{10}\) in a lower temperature range 650–980°C. The growth of the oxidised phases was measured at temperatures below the α/β transus, i.e. 871°C and also in the α + β range i.e. ~ 871–980°C. Below the β transus the oxidation, although remaining parabolic, deviated from extrapolation of data obtained at higher temperatures. The temperature dependence did not conform to an Arrhenius relationship over the range 871–980°C i.e. the α + β region. At temperatures above and below the α + β range of coexistence in equilibrium, the parabolic rate constants obeyed an Arrhenius relationship, the activation energy being much lower in the lower temperature range 650–820°C. The measured rate constants are shown as a function of reciprocal temperature in Fig. 2 and the Arrhenius temperature dependence of the parabolic rate constant for the lower temperature range is listed in Table 1. The temperature dependence of the growth rate constants for the oxidised layers are listed in Table 2.

The parabolic rates of reaction over the temperature range 973–1251°C have been measured by Westerman and Hesson\(^{11}\) in 7.5 g min\(^{-1}\) flowing steam in an induction furnace. The extent of reaction was measured by metallographic measurement of oxidised phase widths and by measurement of the product hydrogen evolved. The temperature dependence of the parabolic rate constants derived from this work are shown in Fig. 3, where the results are compared to other work from JAERI.\(^{12}\) The temperature dependence of the parabolic rate constants is also listed in Table 1. Westerman and Hesson also measured the hydrogen uptake in the oxidised samples and reported 220–250 ppm hydrogen which is in excess of that reported by other workers\(^{9,13}\) typically 20–30 ppm.

Kawasaki\(^{12}\) used gravimetry and phase thickness measurements to determine the rates of reaction of Zircaloy–4 exposed to 0.4 g cm\(^{-2}\) min\(^{-1}\) flowing steam in a resistance furnace over the temperature range 1000–1330°C. Steam flow was found not to be rate limiting above 0.18 g cm\(^{-2}\) min\(^{-1}\) and the oxidation followed a parabolic rate law at temperatures of 1000°C and above, deviating from a parabolic law at 900 and 950°C. The temperature dependence of the rate constant describing oxygen uptake is plotted in Fig. 3 and listed in Table 1. The temperature of the rate constants describing the growth of the oxide and α-layers are listed in Table 2.

A detailed and comprehensive study of the oxidation of Zircaloy–4 in
steam at temperatures between 900-1500°C was performed by Pawel et al. The very fast heating and cooling of small sections was achieved in a radiant infra-red furnace and temperature measurements were made with a great degree of precision. Steam flowed up the reaction tube at 1 m s⁻¹ and measurements were made of phase widths and weight gain. The oxide and α-phase grew parabolically at temperatures above 1000°C, below this temperature the oxide phase deviated from parabolic behaviour and tended towards a cubic growth rate, thought to be a feature of the tetragonal to monoclinic phase transformation. The parabolic rate constants derived from this work are reproduced in Tables 1 and 2 and compared to other data in Fig. 2.

The weight gain of Zircaloy-2 specimens exposed to steam in the temperature range 1000-1200°C was measured by Brown and Healey. The specimens were heated in a tube furnace and cooled rapidly by quenching. Different oxidation behaviour was observed above and below 1200°C. Above this temperature the oxide film was duplex whereas only occasionally was a duplex film formed below 1200°C. In common with many other workers the authors report the occurrence of a tin-rich layer of precipitate particles at the interface of the two types of oxide layer above 1200°C. The weight gain data was analysed as conforming to a parabolic rate law of oxidation and the rate constants for single layer and double layer oxide growth are listed in Table 1 and compared to other data in Fig. 4.

The kinetics of both Zircaloy-2 and Zircaloy-4 oxidised in unlimited steam were measured by Urbanic and Heidrick in the temperature range 1150-1850°C using an induction furnace. Phase boundary movement, hydrogen evolution and weight gain were measured and yielded parabolic oxidation rates over the whole temperature range. However, at ~1580°C the parabolic rate constant increases sharply with only a small change in activation energy.

The increase is due to a change in the kinetics of the formation of the oxide phase associated with the tetragonal to cubic phase transformation in zirconia. The rate of growth of the α-phase remaining at that which would be extrapolated from the lower temperature region. The temperature dependences of the oxidation rate constants are listed in Table 1 and compared to other data in Fig. 4.

A number of measurements of oxide and alpha-layer formation during transient heating have been performed by Sagat et al at Chalk River. The parabolic rate constants determined during transient heating agree closely with those of Leistikow for isothermal conditions (see Table 2).

The most extensive investigation of the kinetics of the Zircaloy-steam reaction has been performed over the last few years by Leistikow, Schanz and co-workers at KfK. Initially, the rates of reaction were measured for short exposure times, up to 15 mins, in the temperature range 700-1300°C. Zircaloy-4 samples were heated in a flow of steam, 1 ms⁻¹ in a resistance furnace and the extent of oxidation estimated by both gravimetry and oxidised phase width measurements. The reaction obeyed a parabolic rate law at and above 900°C but below 900°C tended to a cubic rate law. Limited observations of oxidation at longer times at 1000°C showed an accelerated rate of reaction associated with fissuring of the oxide film. The isothermal parabolic rate constants for total reaction
derived from this work are compared with other data in Fig. 2 and listed in Table 1. The growth constants for oxide and alpha-layer are also listed in Table 2. The published oxidation rate constants are established from the data in the temperature range 1000-1300°C but are reported to be a good approximation over the temperature range 800-1500°C.

The reaction has also been investigated at temperatures up to 1600°C. Zircaloy-4 cladding tube sections were exposed isothermally in flowing steam at 50°C intervals between 1300 and 1600°C. A dense protective columnar grained oxide scale was formed in all tests which varied in time from 60 mins at 1300°C to about 6 mins at 1600°C and in all tests a parabolic rate law was obeyed. However, at 1550 and 1600°C the parabolic reaction rates were considerably higher than the extrapolation from lower temperatures. The microstructure of Zircaloy oxidised at 1550 and 1600°C exhibited a dual layered appearance, the inner layer of zirconia containing stringers and precipitates of α-ZrO(2) along crystallographic directions and grain boundaries. The inner oxide is thought to have been cubic zirconia at temperature which transforms on cooling by a eutectoid decomposition to the stable monoclinic ZrO₂ and α-ZrO(2).

The parabolic rates of reaction for oxygen uptake at 1300 to 1600°C are plotted in Fig. 4 and compared with the measurements of Baker-Just(7) and Urbanic and Heidrick.(15) Chung(20) also measured the zirconia scale growth at temperatures above and below the nominal temperature of the tetragonal to cubic zirconia phase transformation and observed an increase in the parabolic rate constant at ~ 1580°C with a similar activation energy in both temperature regimes. Fig. 5 compares the zirconia scale growth kinetics of Urbanic and Heidrick,(13) Leistikow et al.(19) and Chung and Thomas(20) at temperatures above 1580°C with the Baker-Just(7) correlation converted to equivalent ZrO₂.

Leistikow and co-workers have also investigated the kinetics of Zircaloy-steam reactions after times much greater than previously reported, i.e. for times up to 25 hours. The temperature range of investigation was from 700°C up to 1600°C and since the Zircaloy test pieces were from cladding tube the times at higher temperatures were limited to those at which complete consumption of the tube wall occurred, e.g. ~ 6 mins at 1600°C and ~ 40 mins at 1500°C. The zirconia scale and the α-layer growth are shown in Fig. 6 as functions of time and temperature along with earlier data up to 15 mins exposure time. Above ~ 1050°C the oxide scales remain protective and the kinetics remain parabolic, however at lower temperature there is significant acceleration of oxidation rate at longer times. The kinetics of α-layer growth remain constant until consumption by conversion to zirconia.

**Breakaway Oxidation of Zircaloy**

Leistikow et al.(19) reported that the kinetics of the Zr-4/steam reaction in the temperature range 600-800°C change from cubic to linear with time, before finally exhibiting a decreased rate of reaction due to the finite thickness of the specimen. The weight gain at which accelerated oxidation begins increases with temperature. The microstructure of Zircaloy oxidised for long times between 600-800°C exhibits numerous lateral fissures and penetrating radial cracks. It is postulated that the oxide cracking is the cause of the accelerated
oxidation which is analogous to the breakaway oxidation phenomena reported for lower temperature aqueous and gaseous oxidation.

In the interval between 850-950°C, oxide cracking was observed to be much reduced and transition to a breakaway accelerated oxidation rate did not occur although there was an increase in rate to a parabolic rate of oxidation. At 1000-1050°C the breakaway type of accelerated oxidation re-occurs. Leistikow et al.(19) also report that under transient oxidation conditions, oxide scales showed breakaway above the critical oxide thickness, but recovered at a temperature where the oxide thickness was less than critical and the conclusion was made that the isothermal breakaway data could be incorporated into transient calculations.

Consideration of the mechanism of breakaway oxidation was initially concerned, with breakaway phenomena at lower temperatures in aqueous media or in some cases in gaseous media. Subsequently several studies have been made of a wide range of zirconium based alloys in different oxidising media and a temperature range up to ~800°C. Cox(21) summarised the conclusions of many investigators and described two alternative mechanisms of breakaway oxidation. One mechanism is based on the hypothesis that cracking of the oxide film, caused by the stresses generated in growing oxide films, is the primary causes of a transition to accelerated corrosion. The alternative mechanism is that the stresses induced in the oxide cause recrystallisation and pore generation in the oxide film, the pores promoting the formation of cracks. However, the precise mechanism of the transition from cubic/parabolic kinetics to linear kinetics has not been established to the general satisfaction of all workers in the field.

More recently Leistikow et al(19) and Schanz and Leistikow(22) have considered the mechanism of breakaway oxidation of Zircaloy in steam at temperatures above ~700°C, as illustrated in Fig. 7. After the initial growth of the tetragonal polymorph of zirconia, stabilised at a lower temperature by stress in the growing oxide film, it is postulated that the tetragonal to monoclinic zirconia phase transformation (Martensitic) is initiated by a combination of stress relaxation, columnar grain coarsening and point defect recombination. At this stage of the process a scalloped oxide/metal interface is observed which is due to the formation of pores at the interface thus locally retarding the oxide growth. The oxide then becomes more brittle as a result of oxygen saturation and lateral cracks form followed by vertical penetrating cracks. The short circuiting of the oxidation path by such cracking leads to the higher rates of oxidation (linear) observed and this cycle is repeated with the initiation of fresh oxide formation. The oxidation response of Zircaloy in the temperature range 850-900°C does not conform to the above pattern of behaviour. A transition of oxidation rate is observed but it is a less abrupt change to parabolic kinetics. This behaviour is not understood but factors suggested as having an influence on the reaction kinetics in the temperature range 850-900°C are the two phase (α+β) matrix and hydrogen uptake of the Zircaloy.

**Effect of irradiation on high temperature Zircaloy oxidation**

The measurements of oxidation kinetics described above have been determined using virgin Zircaloy samples in unlimited steam, in the absence of radiation. Reactor cladding subject to high temperature transients in accident situations will begin the transient with a range of
in-service histories and although the neutron flux will be minimal a Y-irradiation field will be present. The effects of the deposition of energy from Y-radiation in Zircaloy or in the steam phase on the oxidation rate of Zircaloy in conditions typical of a LOCA have not been systematically investigated. However, the effect of irradiation on the oxidation of Zircaloy's and other zirconium based alloys in water and steam at lower temperatures has been extensively investigated by numerous workers and reviewed by Johnson,23 Cox21 and Asher.24 Enhanced corrosion has been observed in water in the presence of a fast neutron flux. In steam, even with added oxygen, the in-reactor enhancement is only very small and is related to the effects of neutron irradiation on the transport properties of the solid oxide/metal rather than the gas phase. It is further concluded that these small effects vanish completely at 400-450°C possibly due to thermally activated recovery processes.

The influence of gas-phase or surface effects on the oxidation of Zircaloy tend only to decrease the oxidation rate by mechanisms producing either a reduction of available oxidising species for incorporation into the oxide lattice or a reduction in the oxygen anion concentration gradient within the oxide. Such mechanisms may arise from simple steam starvation or by effects produced by high hydrogen concentration (see Section 5.2.4).

For Zircaloy oxidation in unlimited steam the rate limiting process is generally believed to be the transport of oxygen anions in the n-type anion deficient oxide film by a process involving point defects. A perturbation of the defect population by the radiation field after a LOCA would be negligible. Thus no measureable influence of radiation on Zircaloy oxidation rates LOCA is predicted.

Effect of specimen geometry

Pawel and Campbell25 have pointed out that initial parabolic kinetics only apply whilst the cladding behaves as a semi-infinite sink for oxygen. Experimental work at 1300°C and calculations for oxidation in the range 1000-1500°C has shown that once the B-phase begins filling with oxygen the kinetics of oxide and a-phase growth deviate from the initial parabolic rate and become faster, the effect being much larger on the a-phase growth kinetics. At 1300°C one sided oxidation experiments showed that the initial parabolic rates were maintained for ~ 500 s but thereafter the rate of boundary movements increased; the cladding original oxygen concentration was 0.12 w/o and the wall thickness 0.0635 cm. On saturation of the B-phase the growth of the oxide and a-phase would proceed at a new increased parabolic rate until the a-phase began to fill with oxygen. However, the total oxygen uptake is slightly lower since no additional oxygen is being absorbed by the B-phase. For saturated B-phase Zircaloy oxidation the growth constants for the oxide and alpha phases between 1000-1500°C were calculated to increase by ~ 3~ 17% and ~ 45~ 135% respectively. The overall oxygen uptake rate constant decreased by ~ 3~ 13%.

Most cladding will also be oxidised from operation in normal reactor running and will have in some cases a small amount of crud. The effect of certain variables on the oxidation process have been investigated and are considered below.
5.2.2 The behaviour of alloying elements during Zircaloy oxidation

The corrosion resistance of Zircaloy under normal running conditions is structure dependent and is believed to be optimised by heat treatments to produce a fine distribution of second phase particles. The particles are intermetallics precipitated along the edges of the small α-platelets (26) and in Zr-4 are reported to be Zr, Fe, Cr compounds with a Cl14 Zr(CrFe)2 crystal structure and a composition Zr5Fe4Cr2 (27, 28) Zr-4 phase equilibria at high temperatures, 750-1050°C, has been investigated by Miquet and Charquet (26) who determined the number and nature of the phase domains between the α- and β-solid solution states. Four phase domains were observed, aSS + Zr(Fe,Cr)2 up to 608°C; aSS + Zr(Fe,Cr)2 + βSS from 608 to 845°C, aSS + βSS from 845 to 1000°C and βSS above 1000°C. In the α + Zr(Fe,Cr)2 domain, Sn is dissolved in the aSS phase, whilst in the α + Zr(Fe,Cr)2 + β domain Sn is also present in β as well as some Fe, Cr in β. In the α+β domain, Sn has greater solubility in β and in the β domain all the alloying elements are in solid solution.

The subsequent re-distribution of alloying elements in high temperature oxidation has been observed by many workers in the field of high temperature Zircaloy oxidation. Yurek et al (29) characterised the oxide scales in terms of alloying element distribution in Zr-4 oxidised in steam at 1000-1300°C. Prior to oxidation, Sn is homogeneously distributed in α-Zr and Fe, Cr are present as intermetallics Zr(Fe,Cr)2 on the edges of α-Zr platelets. After oxidation the Fe and Cr were observed to be segregated in the prior β-phase. The oxide scale contained a Sn rich intermetallic existing as a line of particles at about the mid-depth of the scale. In the scale outer layer there was little Sn except for a thin layer at the oxide/gas surface. The inner oxide contained columnar grains of ZrO2 with a fine metallic phase at the grain boundaries. During continued oxidation the Sn rich intermetallics move towards the metal. Biederman et al (10) determined the composition of the intermetallic phase in the oxide as Zr4Sn and suggested the grain boundary phase in the inner oxide was α-Zr[0]. Urbanic and Heidrick (15) observed globules and stringers of α-Zr[0] in the oxide scales of Zr-4 oxidised in steam at 1050-1800°C. The volume fraction of α-Zr[0] determined in oxides formed < 1580°C was ~ 2%, consistent with the ZrO2 (tetragonal) + ZrO2 + α Zr[0] eutectoid transformation on cooling at ~ 1000°C. In oxides formed > 1580°C, the volume fraction of α-Zr[0] was ~ 10%, consistent with the ZrO2 (cubic) + ZrO2 (tetragonal) + α-Zr[0] eutectoid transformation on cooling at ~ 1577°C.

The behaviour of alloying elements in Zr-4 transiently oxidised in 80-20 argon-oxygen at higher temperatures i.e. 1600-1800°C was reported by Gambini et al (30). The composition of the intermetallic particles in the oxide scale was determined to be ZrSn1.1-ZrSn1.9. In the α-Zr[0] layer, adjacent to the oxide, two metallic phases were observed. The most abundant phase was of composition near Zr5Sn3, the other being Zr(Fe,Cr)0.5-0.8, both phases containing ~ 10% oxygen after oxidation at 1600°C and ~ 20-25% oxygen at 1700-1800°C. Islands of Zr5Sn were also observed in the growing ZrO2 near the oxide/metal interface. The morphology, distribution and composition of the Zr-Sn intermetallics in the oxide scale were found to be strongly dependent upon temperature and the characteristics of the transient and it is suggested that the Zr5Sn particles observed by previous workers are, at higher temperatures and longer times, transformed to Sn rich liquid droplets by progressive
oxidation of the zirconium component. The formation and migration of liquid droplets is suggested to result in the general transportation of all alloying elements in liquid form towards the inner surface of cladding. The suggestion is also made that a detailed appraisal of the nature and distribution of the intermetallic phases in high temperature oxidised Zr-4 may facilitate the estimation of temperature of cladding in high temperature severe fuel damage experiments.

5.2.3 High temperature oxidation of pre-oxidised Zircaloy

Zircaloy cladding oxidises slowly during normal operation of an LWR. Many out of reactor studies have been made of oxidation in the temperature range appropriate to normal running and some in-reactor measurements have also been made.\(^{(31)}\) The initial oxidation rate in reactor is cubic (100-500 days) and when the oxide is a few microns thick (\(\sim 2-5\ \mu m\)) the oxidation rate becomes linear owing to fissuring of the oxide film. At the end of life, fuel cladding may have 5-50 \(\mu m\) of oxide depending on service conditions, (see Fig. 8). The waterside corrosion of Zircaloy-4 in the hydrogen overpressure environment of a PWR is not a fuel life or performance limiting feature in present day reactor operation. There are however, economic and environmental pressures to extend the burn-up of fuel beyond the current target of \(\sim 30\ GWD/t\) up to \(\sim 50\ GWD/t\), namely the more efficient use of uranium and of fuel storage facilities. There is also an incentive to maintain a high coolant pH to minimise the transport of highly activated corrosion products and hence minimise man-rem doses.

Oxidation of Zircaloy may be enhanced in extended burn-up simply by increased exposure time. Additionally a thicker oxide film with associated poor heat transfer characteristics, raises the oxide/metal interface temperature and further increases the oxidation rate. Data is also available which demonstrates the enhanced corrosion of Zircaloy in very high pH (high Lithia) water. The effect is thought to occur from hide-out and concentration of lithia in porous oxide.

The effect of lithia on oxidation of Zircaloy in LOCA conditions is not reported but the subsequent high temperature steam oxidation of pre-oxidised Zircaloy (albeit mostly at higher temperatures than reactor operating) has been reported by a number of workers.

The high temperature oxidation of Zircaloy with pre-formed oxide has been studied by Leistikow et al\(^{(17)}\) who pre-formed oxide in the range 350-600°C up to 50 \(\mu m\) thick prior to further oxidation at temperatures of 1000 and 1200°C. The authors concluded that the pre-formed oxides were generally protective if the further oxidation did not exceed 1200°C. However, beyond this temperature the more highly defective tetragonal zirconia formed and the protective action of the pre-formed oxide decreased. Leistikow et al\(^{(32)}\) also pre-formed oxides \(\sim 30\ \mu m\) thick in the temperature range \(\sim 400-800^\circ C\) and exposed the samples to transient high temperature oxidation. The authors concluded that pre-oxidation did not affect subsequent high temperature corrosion systematically and that present data did not provide a sufficient basis for an analytical description of the high temperature transient oxidation of pre-corroded Zircaloy.

Zircaloy pre-oxidised at 871°C for 110 s and 500 s to give 5 \(\mu m\) and
10 μm oxide film was oxidised in steam isothermally at 980–1315°C by Biederman et al. (10). Oxidation at high temperature continued to obey a parabolic rate law although as shown in Fig. 9 the amount of oxidation became increasingly less than that expected on virgin cladding as the isothermal oxidation time increased. However this treatment is not self-consistent. In parabolic square root representation pre-oxidised material gives no straight lines unless the pre-formed oxide is completely non-protective. On the other hand, reduced slopes, which means reduced reaction rate coefficients, are only possible if a moderating influence even on the rate limiting mechanism of the oxidation, that is the boundary values or profile of the anion vacancy concentration of the ZrO₂ scale, is assumed.

Kawasaki et al. (12) also pre-corroded Zircaloy in pressurised water at 300°C to give an oxide film ~ 1 μm thick. Subsequent oxidation in steam at 900 and 950°C showed a protective effect i.e. less oxidation for up to 50 mins at these temperatures.

Iglesias (33) has pointed out that tests with pre-oxidised Zircaloy have been interpreted as having 'protective' oxide for temperatures below 1200°C. Only three points in Figure 9(b) (2000°F and 1800°F for t > 16) support this conclusion. If specimens are pre-oxidised, the oxygen gradient produced in the stabilised α-layer dictates the speed of the oxide/α interface during further oxidation at different temperature. As the oxygen gradient adjusts by diffusion to be aligned with the new oxidation temperature, parabolic kinetics will be re-established. At high temperatures (>1200°C), oxygen diffusion is so rapid that concentration gradient re-alignment occurs easily and little effect of pre-oxidation is noted. Such an effect cannot be represented using parabolic rate laws but is accounted for using moving-boundary diffusion models (see Section 5.4.2).

5.2.4 High temperature oxidation of Zircaloy in impure steam

The oxidation of Zircaloy at high temperatures in oxidising media other than pure steam has been investigated for a range of conditions. Small amounts of impurity, N₂, O₂, up to 10% and H₂ up to 5% were added to steam and the Zircaloy oxidised at temperatures of ~1100 and 1300°C by Cathcart et al. (34) but without any significant effects on the oxidation rate.

Zircaloy was exposed to pure N₂, O₂ and air by Leistikow et al. (17) in the temperature range 900–1150°C. The results are shown in Fig. 10 and show that the weight gains are slightly higher for oxidation in air and oxygen than in steam. In air zirconium nitride forms which is itself oxidised permitting further easier access of oxidant. In oxygen, there are no gaseous reaction products and therefore no perturbations to the flux of oxidising species arriving at the oxidised surface.

Leistikow, Kraft and Pott (35) also oxidised Zircaloy tubes in steam and air in comparative stress rupture tests. It was concluded that at temperatures up to ~ 800°C the oxidation characteristics in air and steam were similar as were the mechanical properties in stress rupture. However, at ~ 1000°C although the oxide formed in steam remained adherent and protective and delayed rupture, the oxide formed in air was extensively cracked and conferred no strengthening in stress rupture. The
mechanism of enhanced corrosion in air at temperature \( > 1000^\circ C \) was the dissolution of nitrogen and the formation of zirconium nitride which is highly reactive, brittle and has a high Pilling-Bedworth ratio.

5.2.5 Influence of hydrogen on Zircaloy oxidation

All previously published measurements of Zircaloy oxidation have been made with flows of steam such that the reaction never became gas phase diffusion limited, a condition that was ensured by experimentally determining the minimum flow rate necessary to supply the oxide/gas phase interface with sufficient oxidising species.

In some circumstances, notably in some beyond design basis accidents it is envisaged that the supply of steam to oxidise the fuel cladding will be from the boil-off of water as the core is uncovered. The steam flows convectively up the fuel channels and the oxidation of fuel cladding produces hydrogen which increases in concentration with height above the boiling water. In such circumstances, calculation of the extent of cladding reacted using kinetic data obtained as above, may be unrealistic.

Additionally, in the region of the perforation in ballooned and burst cladding, the steam ingress oxidises the cladding inner surface. The residual hydrogen results in a high hydrogen steam ratio adjacent to certain locations of the inner surface which determines the mode of oxidation and in some circumstances the embrittlement (see Section 5.3.2) at these locations.

The effect of hydrogen on the oxidation characteristics of Zircaloy was noted at \( \sim 750^\circ C \),\(^{(36)}\) the weight gain being lower in an approximately 80% hydrogen-steam mixture. Furuta et al.\(^{(37)}\) observed different oxidation characteristics on the inner diameter of a dummy fuel rod burst and oxidised in flowing steam. Further work by JAERI on the oxidation of burst dummy fuel rods in steam\(^{(38-41)}\) has shown a different morphology of oxide formed on the inner surface and the effects of such oxidation on the mechanical properties of oxidised Zircaloy. Evaluation of the fuel rods after in-reactor LOCA tests in the FR2 reactor, performed by KFK, has identified thicker internal compared to the external oxide layers near the rupture elevation for all high-burn-up fuel rods. Steam consumption, hydrogen enrichment, as well as burn-up influences (growth of defective scales) have been interpreted as contributing to this modified oxidation behaviour.\(^{(87)}\) The effects of hydrogen-steam mixtures on the oxidation of non-deformed Zircaloy has been reported by JAERI.\(^{(42,39,43)}\) Small sections of Zircaloy-4 cladding were heated isothermally in the temperature range 950-1100°C in a flowing mixture of steam and hydrogen. The ratio of hydrogen to steam was varied over the range 0.05-2.0. The total weight gain was found to be a function of flow rate, temperature, time and hydrogen concentration. At a high flow rate of 18.3 cm\(^3\) s\(^{-1}\), and temperatures of 1000, 1100°C there was a slight but definite decrease in the weight gain of specimens oxidised in mixed gas of \( \text{H}_2/\text{H}_2\text{O} \) ratio 0.4 as compared to those in steam. At a flow rate of 0.5 cm\(^3\) s\(^{-1}\), the weight gain as a function of time, temperature and \( \text{H}_2/\text{H}_2\text{O} \) volume ratio is shown in Fig. 11. For temperatures of 1000°C and above there is a critical \( \text{H}_2/\text{H}_2\text{O} \) volume ratio below which the oxidation behaviour is the same as in pure steam, but above which the weight gain exhibits a marked decrease. This critical ratio depends on temperature and flow rate and tends to lower values as the temperature increases from 1000-1100°C.
The effect of hydrogen concentration in mixed hydrogen/steam gas on the hydrogen uptake in oxidised Zircaloy-4 is shown in Fig. 12. Below the critical $H_2/H_2O$ volume ratio the hydrogen uptake is similar to that observed in pure steam. At the critical ratio the hydrogen uptake increases rapidly.

In hydrogen/steam below the critical ratio the oxide structure and morphology is the same as in pure steam e.g. the oxide is compact, dense and in room temperature examination, has a monoclinic crystal structure. Above the critical ratio the increasing hydrogen concentration causes the oxide to form as a porous oxide consisting of both the monoclinic and tetragonal polymorphs of ZrO$_2$. Additionally, a measurement of oxygen distribution in the transformed $\beta$-phase of specimens oxidised for 1200 secs at 1000°C showed a lower oxygen concentration in the specimen oxidised in a mixed gas of $H_2/H_2O$ ratio above the critical ratio and which contained 3230 ppm hydrogen.

The authors conclude that hydrogen in steam can have a marked effect on the oxidation characteristics of Zircaloy-4 in steam hydrogen mixtures. The weight gain varies with the $H_2/H_2O$ volume ratio at temperatures of 950-1100°C and the effect is more marked as the flow rate decreases. The critical value of the $H_2/H_2O$ ratio depends on temperature and flow rate. The tetragonal oxide associated with oxidation in gas mixtures above the critical $H_2/H_2O$ ratio is promoted by the absorbed hydrogen observed in such samples. The oxygen concentration profile in transformed $\beta$-Zircaloy can be influenced by hydrogen uptake and the occupancy by the hydrogen of the octahedral sites in ZrO$_2$.

Chung and Kassner(44) and Chung and Thomas(20,45,46) have also investigated the effects of hydrogen/steam mixed gas oxidation of Zircaloy mainly to study the implications to core heat up in degraded core type accidents where hydrogen in steam and reaction kinetics above 1200°C are particularly significant. Chung and Kassner(44) observed anomalous oxidation on the inner and outer surfaces of Zircaloy cladding oxidised in steam. Several types of specimens were examined including Al$_2$O$_3$ pelleted, pressurised and ruptured cladding with pellets of various diameter and undeformed cladding with various size holes to admit steam. Significant variations in oxide and $\alpha$-phase thicknesses were frequently observed in ruptured cladding which were not attributable to local temperature fluctuations. Two distinct types of anomalous oxide were observed. An oxide observed only on the outer surface is characterised by white contrast under polarised light and is somewhat similar to that observed in the breakaway oxidation regime, (Section 5.2.1). A second porous oxide was observed on both inner and outer surfaces and is characterised by small circumferential cracks and a much larger oxide thickness relative to normal dense oxide. This type of oxide was observed in highly localised areas which suggests temperature or $H_2/H_2O$ ratios are not particularly influential since these would not occur over the small distances involved. The authors suggest local stress variations are more likely to influence the formation of such oxides. Since many interdependent factors combine to determine the precise conditions of oxidation in such type of tests, e.g. local stress concentrations, size of rupture, steam/hydrogen transport, outside and inside the cladding and through the rupture, temperature variations etc. it was not possible to unequivocably establish the relationship between the various factors such as $H_2/H_2O$ ratio on the oxidation characteristics of the Zircaloy cladding.
Chung and Thomas(20,45,46) subsequently performed a series of experiments to investigate the influence of hydrogen in steam on the oxidation of Zircaloys. In the first series of experiments(20,45,46) the oxidation rate of Zircaloys cladding in hydrogen–steam mixtures was measured in the temperature range 1200–1700°C in an overpressure of ~ 33 kPa hydrogen in a total pressure of ~ 38–45 kPa. Steam–helium mixtures were also used to distinguish between rate limitations in the gas phase and surface reactions. The extent of reaction was measured as oxide thickness and the ratio of the parabolic oxide layer growth constants obtained in steam–hydrogen and in pure steam are shown in Fig. 13. It is evident that the oxidation rate is retarded in steam hydrogen mixtures. The retardation is greater for higher hydrogen–steam ratios and for a given hydrogen–steam ratio is greater at higher temperatures. In pure steam the oxidation rate was always parabolic and similar to previously reported data, hence steam starvation was not occurring in these tests. Oxidation in hydrogen–steam mixtures is parabolic or linear depending on the specimen temperature, the pressures of steam and hydrogen at the specimen surface and the time of oxidation i.e. the oxide layer thickness. Initial oxidation kinetics in hydrogen–steam were linear rather than parabolic at the lower steam supply rates and were lower indicating either gas phase diffusion or surface reaction limitations e.g. dissociation of water molecules or subsequent reduction of oxidising species. Since oxidation in steam–helium mixtures in similar conditions was observed to be parabolic, on the assumption that steam has similar diffusivities in hydrogen and helium it is concluded that the rate limiting process in steam–hydrogen is a surface reaction.

Eventually as the oxide film thickens, the flux of oxidising species at the oxide/metal interface decreases, solid state diffusion becomes rate limiting and parabolic kinetics are obeyed. In these circumstances lower oxidation rates than would be achieved in pure steam were observed and it is postulated that hydrogen dissolved in the cladding is responsible for decreased oxygen diffusion. It is suggested that this is due to interstitial or substitutional $H^+$ ions producing an increase in the oxygen vacancy concentration in the oxide near the gas/specimen surface thus decreasing the oxygen vacancy concentration gradient across the oxide and hence the oxygen diffusion rate.

The limitations of the former experiments in adequately quantifying the effects of hydrogen on Zircaloys oxidation in the realistic conditions of flowing steam were subsequently stated by Chung and Thomas.(20) In the above experiments, the steam molecules impinge on the oxide by natural convection in a static overpressure (~ 33 kPa) of hydrogen. Steam impingement rate varies by virtue of a controlled steam supply rate, a decreased supply rate implying an increased hydrogen molar fraction. Under these conditions the oxidation rate is a function of steam supply rate as well as temperature and it is not possible to quantify the steam and hydrogen partial pressures at the reaction rate. Chung and Thomas(20) reported further experiments in which a shroud was placed around the Zircaloys cladding and flowing steam–hydrogen mixtures were passed over the Zircaloys. A flow of hydrogen was maintained at 10 L/min and the steam supply varied between 0.5–3.2 g/m to give hydrogen molar fractions ranging from 0.94 to 0.71. The ratio of the oxide layer growth constants for the hydrogen–steam and pure steam are shown in Fig. 14 as a function of cladding temperature. The lower parabolic rates observed in steam hydrogen mixtures are again interpreted as being due to hydrogen.
entering the zirconia lattice either as interstitial of substitutional H⁺ ions. Confirmation that hydrogen was contained in the surface oxide was obtained by powdering the surface layers and chemical analysis. The analysis showed that the oxide formed in hydrogen-steam mixtures contained a significant quantity of hydrogen in the outer layer, having a composition which approximates to ZrOH. Zircaloy oxidised in pure steam contained little hydrogen < 0.03 w/o but as the hydrogen-steam ratio increased the hydrogen content of the outer oxide increased and the oxygen content decreased.

The fraction of the total hydrogen generated which becomes dissolved in the cladding can be significant as shown by Fig. 15. The data derives from tests on pressurised ballooned ruptured and oxidised Zircaloy tubes in simulated LOCA's(44) Anomalous porous oxidation generally observed on the inner diameter at < 1140°C is responsible for the large dissolved hydrogen fraction at ~ 1140°C. Chung and Thomas(20) point out that such data demonstrate the importance of Zircaloy cladding as a sink for hydrogen and also point out that further data are required for a satisfactory understanding of all the features associated with hydrogen-steam oxidation of Zircaloy.

Chung and Thomas(47) analysed the implications of high hydrogen concentrations on core heat-up in beyond design basis accidents and although more data are required on oxidation rates in high hydrogen/steam ratios before a quantitative analysis is possible, it was concluded that, during prolonged core uncovery the cladding oxidation rate will be lower and therefore the core-heat-up rate will be lower. Additionally, the lower oxidation rate should not affect embrittlement behaviour since this will be established before the high hydrogen concentrations have an effect. Finally, less liquid UO₂ and debris will be formed than would be predicted using unlimited steam oxidation kinetics.

However, more recent data from Prater and Courtright(203) conflicts with that reported above in that oxidation in steam - hydrogen mixtures between 1300°C and 2400°C has failed to show any influence of hydrogen on the initial parabolic growth rates of the ZrO₂ and α-Zr[0] phase thicknesses until the hydrogen concentration reaches ~ 90 mol %. At this high concentration the gas-phase mass-transfer of steam molecules is reduced to a flux less than that required to oxidise Zircaloy at the rate determined by solid-state diffusion.

5.2.6 High temperature oxidation of Zircaloy in high pressure steam

The majority of measurements on the oxidation of Zircaloy in steam have been carried out in ambient pressure steam. For parabolic oxidation, the rate controlling process is diffusion of anion species. Theories of parabolic oxidation based on diffusion predict rates independent of pressure. Pawel et al.(48) have measured the oxidation of Zircaloy in steam at pressures up to 10.34 MPa (1500 lb/in²) and at temperatures of 900 and 1100°C for times up to 2500 and 500 s respectively. Measurements of the oxide and α-phase thickness showed that at 1100°C there was no effect of pressure but at 900°C, increase in steam pressure tended to increase the thickness of the oxide phase. The authors were unable to postulate a mechanism for the observed effect but suggested that the formation of fine cracks in the outer layer of oxide may enhance oxidation at high pressure.
However, an alternative explanation of enhanced oxide formation during oxidation at 900°C at high pressure may be the increased transformation of the oxide to the more stable tetragonal oxide in which oxygen transport is greater than in the lower temperature monoclinic form.\(^{(49)}\)

### 5.2.7 Effect of deformation on Zircaloy oxidation

Cladding may undergo simultaneous deformation and oxidation during a LOCA until the cladding ruptures. The deformation increases the available surface for oxidation hence increases the volume of oxide formed. Since deformation generally occurs at lower temperatures than those significant for oxidation little work has been done to measure explicitly the enhancement of oxidation in Zircaloy arising from simultaneous deformation and oxidation.

Knights and Perkins\(^{(50)}\) reported that under certain conditions i.e. > 400°C and an oxide film thicker than ~ 2 μm, an applied tensile stress in the creep range increased the oxidation of Zircaloy-2 in steam and in wet oxygen but not in pure oxygen, up to 475°C. The enhancement of the oxidation rate varied between 1.2 to 2.0 and was attributed to a stress induced inhibition of the parabolic to cubic kinetic transition.

Bradhurst and Neuer\(^{(51)}\) compressed Zircaloy rings in flowing steam at 700–1300°C. It was concluded that deformation imposed during high temperature oxidation could significantly increase the amount of oxidation. However in such inhomogeneously strained specimens the effects of deformation were local and overall weight gain was only slightly influenced. The enhanced oxidation takes the form of increased local penetration beneath oxide cracks and for similar time, temperature exposures the effect was more marked at a slow as opposed to a fast strain rate. The maximum enhancement factor observed was about two.

Simultaneous oxidation and creep at 900°C was investigated by Leistikow and Kraft\(^{(52)}\) who pressurised sections of sealed cladding at various pressures and exposed them to either argon or steam for up to 30 mins. The strain to failure was found to be dependent on strain rate and was lower for tubes ruptured in steam due to the strengthening effect of the oxide film. A comparison of oxide thicknesses on strained and unstrained tubes showed that the amount of oxidation increased with increasing stress. The enhancement was attributed to oxide cracking and the provision of new surface for oxidation rather than any influence of deformation on the basic oxygen transport mechanisms through the oxide film.

Furuta and Kawasaki\(^{(53)}\) studied the oxidation of axially strained to failure tensile specimens simultaneously oxidised in steam at temperatures between 700–1000°C and a strain rate of ~ 2.8 × 10^{-2}s^{-1}. An increased oxide thickness was observed in deformed specimens, the enhancement being greater with increasing strain but decreased with increasing temperature. The maximum enhancement observed was ~ 1.3 in the ruptured area at 700°C and after 300 s exposure. No observations on oxide morphology or the nature of the oxidation enhancement were reported.

More recently Parsons and Hand\(^{(54)}\) have oxidised in flowing steam pressurised Zircaloy-4 cladding at ~ 710°C and ~ 150 lb/in²g and ~ 800°C
and ~ 80 lb/in²g. The oxidising cladding simultaneously deformed (swelled) and tubes were withdrawn from the furnace at various intervals with diametral strains ranging from ~ 5% to ~ 30% overall but up to 80% local strain at the perforation. At ~ 710°C, unpressurised control cladding oxidised initially at a cubic rate before a transition to an approximately linear rate. The deforming cladding very quickly oxidised at a near linear rate with an enhanced weight gain which increased with increasing strain giving a weight gain ~ 2–2.5 times greater than unpressurised cladding after this exposure. At 800°C unpressurised cladding continued to oxidise at a cubic rate up to 7 h but pressurised cladding exhibited a transition to a near linear rate after ~ 5 h after which the weight gain was enhanced by ~ 2½–3 times compared to unpressurised controls.

The oxide thickness was also measured and shows the same general enhancement as the weight gain. The oxide is extensively cracked at strains greater than ~ 10% at ~ 710°C and ~ 5% at ~ 800°C and beneath the cracks there is an increased penetration of α-Zr[0]. The increase in total oxidation is in part due to the increased surface area on deformed (swollen) cladding but the increase in oxide thickness compared to the undeformed cladding suggests that the enhanced oxidation is also a consequence of increased access of oxidant to the underlying metal by virtue of cracking of normally protective oxide.

5.2.8 Stress and dimensional changes in oxidised Zircaloy

Since the molar volume of ZrO₂ is appreciably greater than that of zirconium i.e. the Pilling Bedworth ratio is ~ 1.5, significant compressive stresses may be generated in the growing oxide scale. These stresses and tensile stresses generated in the metal from dilution by oxygen solution can result in significant tensile stresses on the metal substrate. Although various mechanisms may relieve the induced stresses e.g. oxide plasticity, oxide cracking, metal plasticity the nett result will be dimensional changes in composite oxide-metal Zircaloy materials. The deformation of 0.7 mm thickness Zircaloy-2 sheet has been measured by Donaldson and Evans(55) in Zircaloy oxidised in steam in the range 800–1210°C. Typical strains were about 1%, the maximum observed being ~ 3% after 300 mins at 900°C. It was established that the dilation produced by oxygen solution in α-Zr has a large effect on stress distribution in the metal substrate and hence influences the average stresses within the oxide. The tensile deformation at high temperatures is attributed to the hydrostatic compressive stresses brought about by dilation due to oxygen solution rather than the stresses produced by oxide growth, the latter being accommodated almost wholly by outward growth.

For short times at lower temperatures, oxygen solution and associated stresses are necessarily small and nett compressive stresses from the oxide (~ 1300 MN/m³) result in metal substrate deformation. With increased temperature and time the oxygen solution stresses grow and eventually dominate, exerting a nett tensile stress on the oxide of 200, 300 and 150 MN/m² at 800, 900, 1210°C respectively. Further oxidation allows relaxation and the stresses eventually become compressive and the metal substrate deforms by creep at lower stress levels, typically 100 and 10 MN/m² at 900 and 1210°C. The authors also report that the stress levels generated in the oxide are not sufficient to influence the basic oxygen transport mechanisms in the oxide scale.
Hammad et al.\(^{(56)}\) measured the deformation of Zircaloy-4 tubes oxidised in air at temperatures up to 900°C. Hoop and axial stresses are generated in oxidising tubes and both radial and axial deformation was observed. The time dependency of strain was initially parabolic becoming linear coincident with the onset of breakaway oxidation. The radial strain varied from \(\sim 1.3\%\) after long exposure at 650°C to \(\sim 7\%\) after \(\sim 10\) hours at 750°C, the axial strain tended to follow the same trend as radial strain but was of a lower magnitude. However oxidation of Zircaloy in air has only limited comparability to that in steam, Leistikow et al.\(^{(35)}\) showed that the oxidation characteristics of Zircaloy in air and steam were similar at temperatures up to 800°C. However at 1000°C in air, the formation of brittle zirconium nitride with a high volume expansion, substantially altered the morphology, kinetics and stresses generated in the oxide film. Thus at temperatures above \(\sim 800°C\) the oxide scales were cracked and the simultaneous nitriding reaction induced much faster rates of oxidation.

Aly\(^{(18)}\) measured the radial and axial strain in Zircaloy-4 cladding tubes oxidised in steam in the range 1350-1600°C. The time dependency of radial strain showed an initial non-uniform rate followed by a linear rate. Both the radial and axial strains increased with time and temperature as shown in Figs 16 and 17.

Comparative studies of the oxidation behaviour of Zircaloy-4 and austenitic 15 Cr-15 Ni steel by Leistikow\(^{(194)}\) have included the measurement of oxidation-induced dimensional changes. Oxidation for 6 h at 1200 to 1308°C resulted in an increase of the outer diameter of tube sections of 12% for Zircaloy-4 compared with 10% for the steel. The inner diameter increased by 8% for Zircaloy-4 and decreased by 10% for the steel. The measured increase of wall thickness was 30-60% for Zircaloy-4 compared with 120% for the steel.

5.3 THE EMBRITTLEMENT OF ZIRCALOY FUEL CLADDING

Oxidation of Zircaloy above the \(\alpha/\beta\) transformation temperature results in the formation of inherently brittle phases, zirconia and oxygen stabilised \(\alpha-ZrO_2\) as well as the diffusion of oxygen into the underlying \(\beta\)-Zircaloy phase. The fracture toughness of the \(\beta\)-phase is reduced by the presence of oxygen, hence the fuel tube is embrittled to an extent depending on the degree of oxidation.

When sufficiently embrittled, fuel cladding may fracture on cooling and break up into pieces thus losing a coolable geometry. The action of re-wetting involves the collapse of the vapour film and nucleate boiling commences. This event takes place at a more or less constant temperature, the Leidenfrost point and for Zircaloy rewetted by water, Chung and Kassner\(^{(44)}\) report that rewetting occurs in the range 475-600°C. The change in heat transfer conditions induces large thermal shock forces which fracture the cladding if sufficiently embrittled by oxidation.

The extent of oxidation necessary to embrittle fuel cladding such that it fragments during rewetting has been determined experimentally by a number of workers for a range of cladding dimensions and early work resulted in the formulation of criteria defining the onset of embrittlement in terms of total extent of oxidation and maximum temperature of oxidation. Subsequent work has shown that the
embrittlement of Zircaloy cladding after transient oxidation in LOCA conditions is more accurately described in terms of the distribution of oxygen in the oxidised cladding.

The external pressure on cladding is reduced to near ambient during a large break loss-of-coolant accident and the cladding initially expands away from the fuel. In pressurised transients the cladding is heated under coolant pressure and contacts the UO₂ fuel. The UO₂ is reduced by Zircaloy and the oxygen diffuses into Zircaloy where it forms α-Zr[O] and also diffuses into the β-phase. Thus in pressurised transients oxidation of the Zircaloy cladding occurs on both i.d. and o.d. and both contribute to the embrittlement of the cladding.

If oxidised cladding remains intact after rewetting there is the further possibility that seismic forces or the hydraulic forces during reflood and quenching or the forces associated with fuel handling and transport could fragment the cladding. The response of oxidised cladding to such loadings at lower temperatures than that of thermal shock has also been considered and a criterion for predicting failure has been suggested.

5.3.1 Current embrittlement criteria

The criteria for emergency core cooling systems currently recommended by the NRC were the result of rule-making hearings held January 27 to July 25 1972. The hearings have been summarised by Gottrell. The new criteria adopted by the USAEC on December 18 1973 are as follows and are intended to apply to failure in thermal shock i.e. quenching:

1. Peak cladding temperature - the calculated maximum fuel element cladding temperature shall not exceed 1204°C.

2. Maximum cladding oxidation - the calculated total oxidation of the cladding shall nowhere exceed 0.17 times the total cladding thickness before oxidation. As used in this sub-paragraph, the total oxidation means the total thickness of cladding metal that would be locally converted to oxide if all the oxygen absorbed and reacted with the cladding locally were converted to stoichiometric zirconium dioxide. If cladding rupture is calculated to occur, the inside surface of the cladding shall be included in the oxidation, beginning at the calculated time of rupture. Cladding thickness before oxidation means the radial distance from the inside to outside of the cladding, after any calculated rupture or swelling has occurred but before significant oxidation. Where the calculated conditions of transient pressure and temperature lead to a prediction of cladding swelling, with or without cladding rupture, the oxidised cladding thickness shall be defined as the cladding cross-sectional area, taken at a horizontal plane at the elevation of rupture, if it occurs, or at the elevation of the highest cladding temperature if no rupture is calculated to occur, divided by the average circumference at that elevation. For ruptured cladding the circumference does not include the rupture opening.

3. Maximum hydrogen generation - The calculated total amount of hydrogen generated from the chemical reaction of the cladding
with water or steam shall not exceed 0.01 times the hypothetical amount that would be generated if all the metal in the cladding cylinders surrounding the fuel, excluding the cladding surrounding the plenum volume, were to react.

4. **Coolable geometry** - calculated changes in the core geometry shall be such that the core remains amenable to cooling.

5. **Long-term cooling** - after any calculated successful initial operation of the ECCS, the calculated core temperature shall be maintained at an acceptably low value and decay heat removed for the extended period of time required by the long-lived radioactivity remaining in the core.

The experimental work on which this criterion is based has been reviewed and extended by Parsons(59) and by Chung and Kassner.(44) Typical experiments were to heat Zircaloy tubes in steam either isothermally or through a pre-defined transient and at the appropriate time to quench either by spraying or by bottom flooding with cold water. The extent of oxidation was expressed as the fractional wall thickness of Zircaloy which would be converted to stoichiometric zirconia if all the oxygen taken up were combined with Zircaloy to form \( \text{ZrO}_2 \), i.e. a measure of the total \( \text{Zr} \) reacted or total oxygen uptake without regard to the detailed reactions and oxygen distribution in the clad wall. The equivalent metal reacted, as described above for the limited number of tests performed was found to correlate well with survival or failure of the cladding in the thermal shock of rewetting. The correlation is shown in Fig. 18 which contains both the original US data and data derived at Springfields Nuclear Laboratories on SGHWR sized cladding. The US data shows a clear division between fractured and intact cladding above and below 17% equivalent metal reacted and hence 17% EQMR (equivalent metal reacted) was adopted as the embrittlement criterion. An additional criterion was also imposed, that the maximum temperature should not exceed 2200°F. The condition arose from the work of Hobson and Rittenhouse on the oxidation and embrittlement of Zircaloy cladding. Hobson and Rittenhouse(60) showed that the growth of the oxide plus \( \alpha \)-Zr\([0] \) phases, \( \delta \), (brittle phases) accelerated above 2200°F (1200°C). Hobson(61) also investigated the effect of the extent of oxidation expressed as \( \text{Fw} \), the fractional thickness of the transformed \( \beta \)-phase, with the deformation response of rings cut from oxidised tubes and tested (flattened) in compression at high (0.25 in/min) and low (0.1 in/min) strain rates. For a given \( \text{Fw} \), the specimens exhibited less ductility as the temperature of the deformation test decreased. The temperature at which the specimens broke in brittle fashion (i.e. zero ductility), the temperature of the ductile to brittle transition, was defined as the zero ductility temperature (ZDT). The Zircaloy tubes were oxidised on both sides for various times in the range 1700-2500°F (927-1371°C) and for the specimens tested at the higher strain rate there was a good correlation between \( \text{Fw} \) and ZDT. For specimens tested at the slower strain rate there was a correlation for specimens previously oxidised up to 2200°F (1204°C) but specimens oxidised at 2400°F (1316°C) were brittle at all values of \( \text{Fw} \). Hobson concluded that the embrittlement of Zircaloy cladding is determined by the concentration and distribution of oxygen in the \( \beta \)-phase which is influenced to some extent by the degree of formation of \( \alpha \)-incursions.

Further evidence(59) that the fracture toughness of Zircaloy is not
uniquely dependent on total oxidation was obtained with SGHWR cladding with pre-formed nodular oxidation which survived quenching even though the equivalent metal reacted was 20%. The oxide and α-Zr[0] phases are extremely brittle, therefore the ability of cladding to resist the shock forces depends on the ability of the β-phase to remain intact, which in turn depends on the profile of diffused oxygen in the β-phase.

The particular character of a transient i.e. the parameters of time at temperature during a LOCA, determine the eventual location of the oxide and α-Zr[0] phase boundaries and also the amount and distribution of oxygen in the β-phase. Hence, the resultant mechanical properties of the cladding depend on the details of the transient i.e. rates of heating and cooling as well as time at maximum temperature and since the total extent of reaction is a measure of oxidation which does not necessarily reflect the above consideration, it was necessary to determine fracture behaviour in terms of the oxygen distribution.

The above discussion refers to the response of cladding to fracture from the thermal shock forces of re-wetting at the temperature of the Leidenfrost point. The ability of cladding to remain intact when subject to the forces of seismic, hydraulic and handling loads at ambient temperature, after a LOCA is a separate but related issue.

5.3.2 Embrittlement and distribution of oxygen in the cladding wall

Pavel(62) used ideal models of diffusion to calculate the oxygen concentration profiles, the fractional saturation and the mean oxygen content in the β-phase of the oxidised specimens used by Hobson.(61) The observed embrittlement was correlated with the calculated oxygen in β-phase parameters and it was suggested that the critical criteria for the onset of room temperature brittleness was 0.7 W/o average oxygen or 95% saturation in the β-phase.

Sawatzky(63) measured the temperature dependence of the tensile properties of oxidised Zircaloy-4 as a function of oxygen concentration, cooling rate, maximum test temperature and oxygen distribution. It was found that at temperatures up to 800°C the tensile properties were essentially independent of maximum cladding temperature and cooling rate but were dependent on oxygen distribution through the cladding wall. On the basis of the results obtained, an interim oxygen embrittlement criterion was proposed, that the oxygen content should not exceed 0.7 W/o over at least half the cladding thickness. This criterion is consistent with the previously mentioned suggested criteria and applies to all cladding thicknesses.

The relationship between various parameters which can be considered to express the extent of oxidation of Zircaloy and the fracture behaviour of Zircaloy has been comprehensively studied by Chung and Kassner.(44) Lengths of Zircaloy cladding (200 mm) containing alumina pellets and pressurised with helium were heated isothermally in a flow of steam. The cladding was cooled to below the α/β transformation at a controlled cooling rate and then quenched by bottom flooding with water. The widths of the oxide and α-Zr[0] phases were measured and used along with a model of oxidation as data for a computer code calculation of oxygen profile across the wall of the clad. Several parameters describing oxidation were computed, e.g. equivalent cladding reacted (ECR),
fractional saturation of the $\beta$-phase ($F_B$) fractional thickness of the $\beta$-phase ($F_{W}$) and thickness of the $\beta$-phase containing less than a specified amount of oxygen ($L_{CO}$). The mechanical response of the quenched tubes was correlated with the above parameters and presented as a series of failure maps which are reproduced as Figs 19-22.

The cladding was cooled through the $\alpha/\beta$ transformation temperature either slowly (5 K/s) or quickly (~100 K/s) by direct quenching from the isothermal oxidation temperature and the cooling rate modified the metallurgical structure of the cladding which produced slightly different mechanical behaviour.

The correlation of fracture behaviour and equivalent metal reacted (ECR) shown in Fig. 19 shows that fast cooled cladding is more brittle than slow cooled cladding and that at lower maximum temperatures of oxidation the 17% ECR criterion is very conservative. A single valued criterion is not apparent from the correlation of the fractional thickness of $\beta$-phase ($F_{W}$) or from the fractional saturation of the $\beta$-phase ($F_B$) as shown in Figs 20 and 21. Chung and Kassner concluded that the best correlation was in terms of a residual width of $\beta$-phase containing less than a specified calculated concentration of oxygen ($L_{CO}$ as shown in Fig. 22. Cladding with less than a calculated 0.1 mm of $\beta$-phase with oxygen concentration $< 0.9$ w/o (fast cooled) or $< 1.0$ w/o oxygen (slow cooled) failed on rewetting. This criterion was obeyed irrespective of the wall thickness, overall oxidation or maximum temperature of the oxidation exposure. The authors also report that the location of fracture with respect to the perforation of the fuel tube was temperature dependent. For tubes oxidised above ~1330°C the fracture occurred in the ballooned region whereas at lower temperature a fin-cooling effect reduced the effective oxidation of this area and fracture occurred away from the perforation.

Kassner and Chung(64) also assessed the margin of performance of ECCSs in LWRs against embrittlement criteria based on total oxidation and on oxygen distributions. During blowdown, ballooning and rupture of the clad can occur which results in wall thinning of the clad and the resultant oxidation of the clad will be a function of not only the time at temperature in steam but also the nature of the wall thinning. Hence the embrittlement of the cladding will be influenced by wall thinning. Kassner and Chung calculated the wall thinning for the peak temperature nodes and rupture nodes from double ended guillotine breaks in two PWRs from data on circumferential straining of clad in single pin and multi-rod burst tests. The influence of wall thickness ratio on the embrittlement criteria, equivalent cladding reacted (ECR), thickness of transformed $\beta$-phase with $< 0.9$ w/o oxygen ($L_{0.9}$) was calculated for the peak temperature and rupture nodes and the two types of transient considered during single and double sided oxidation. The results are summarised in Table 3 and show the conservatism in the criterion based on total oxidation in transients not exceeding ~1200°C in that for an average circumferential strain of ~38% and two sided oxidation of the cladding at the peak temperature node, the ECR parameter is 26% and 22.5% i.e. failure and the $L_{0.9}$ parameters is 0.25 mm and 0.28 mm i.e. no failure in either of the transients. For one-sided oxidation neither criterion indicates failure.
5.3.3 Embrittlement below thermal shock temperatures

In thermal shock, the maximum loading occurs in the range 475-600°C. At these temperatures and the cooling rates associated with quenching, hydrogen is in solution and has little effect on the fracture resistance of oxidised Zircaloy. Hydraulic, seismic and handling forces, essentially impact forces, may be imposed at lower temperatures e.g. 100-200°C where dissolved hydrogen or precipitated ZrH₂ may influence the fracture characteristics of oxidised cladding. The volume fraction of hydride precipitated is strongly influenced by the cooling rate. In consideration of possible fragmentation from such forces Chung and Kassner measured the impact, diametral tube compression and tensile properties at ambient temperature of undeformed and deformed and ruptured oxidised cladding which had survived thermal shock. The combined effects of wall thinning, oxidation and hydrogen uptake are represented in the mechanical property measurements. Within the β-phase, as the Zircaloy cools through the β→α and β→α + ZrH₂ transformation, oxygen tends to concentrate in the α-Zr[O] phase and hydrogen precipitates or diffuses to the oxygen depleted regions of the β-phase. It was found that oxygen distribution in the β-phase and hydrogen content of the cladding were the greatest influences on the impact failure properties. The resistance to fracture is determined by the amount of low oxygen β-phase and its ductility which in turn is influenced by the hydrogen content. Data on the combined effect of oxygen and hydrogen on the impact properties of Zircaloy is not available, thus Chung and Kassner correlated the failure in impact with the β-phase thickness, the hydrogen content and the calculated centreline oxygen content of the β-phase. The capability of oxidised cladding to withstand an impact energy of 0.3 J at ambient temperature was found to be best correlated in terms of the β-phase thickness containing < 0.7 w/o oxygen as shown in Fig. 23. The hydrogen concentration in the specimens used for this correlation did not contain > 2200 ppm hydrogen. However the influence of oxygen and hydrogen on the Charpy impact properties of homogeneous Zircaloy containing a range of oxygen and hydrogen contents were measured and these and other data were used to conclude that hydrogen concentration > 2000 ppm along with oxygen would not cause a significant change in ambient impact failure characteristics.

Although the magnitudes of hydraulic, seismic and fuel handling loads have not been accurately estimated, Chung and Kassner consider the magnitude of the 0.3 J impact loading is a reasonable approximation to such post-LOCA low temperature clad loadings.

However, until a quantitative assessment of the lower temperature impact loadings are available and since the impact failure data and measured and calculated properties from the diametral compression tests are essentially an indication of material toughness for a given known load, the low temperature impact criterion is cited as an interim criteria.

In conclusion Chung and Kassner recommend two embrittlement criteria:

1. For cladding to have the capability to withstand thermal shock during reflood - the calculated thickness of the cladding with < 0.9 w/o oxygen based on the average wall thickness at any axial location shall be greater than 0.1 mm.
2. For cladding to have the capability to withstand fuel handling transport and storage - the calculated thickness of the cladding with < 0.7 W/O oxygen based on an average wall thickness of any axial location shall be greater than 0.3 mm.

The above criteria are applicable irrespective of the oxidation temperature, the initial cladding wall thickness and the wall thickness that results from ballooning and deformation.

It should be emphasised that the criteria as suggested are based on oxygen profiles calculated for the isothermal oxidation of the experimental cladding using an equilibrium, ideal diffusion model of oxidation rather than on experimental measurements of oxygen profiles in the residual transformed β-phase of oxidised cladding.

Kawasaki, Furuta and Uetsuka(39-41) have investigated the fracture of cladding oxidised and ruptured in flowing steam in which hydrogen uptake accompanied oxidation. At 100°C the ductility of the cladding was significantly degraded by the hydrogen. Figure 24 shows the relationship between the time-temperature parameters of the oxidation and the associated integrated energies obtained from the load-deflection curves to the point of maximum load (i.e. initial cracking) above and below 0.3 J. These were obtained from diametral tube tests at ambient temperature by Chung and Kassner and from ring specimens compressed at 100°C by Kawasaki et al. The 100°C ring specimens show a lower failure boundary, in terms of time at temperature to achieve failure, than that of Chung and Kassner. However, Chung and Kassner point out that the different experimental techniques of heating, direct resistance (Chung and Kassner) and furnace (Kawasaki et al) result in large temperature differences in the ruptured region by fin cooling of resistance heated tubes and such regions have lower oxygen and hydrogen uptake. If the failure characteristics are evaluated in terms of the material parameters i.e. the oxygen distribution and hydrogen uptake instead of time at an apparent temperature, the failure boundaries are similar.

5.3.4 Comparison of embrittlement in-reactor and out-of-reactor

The embrittlement of in-reactor oxidised and quenched cladding has been studied in the Irradiation Experiments (IE series) and the Power Coolant Mismatch (PCM series) fuel behaviour tests in the PBF reactor at INEL.(65) The fuel rods were subject to cycles of film boiling operation at full coolant pressure and subsequent quenching produced cooling rates ~ 100 Ks\(^{-1}\). Some failures were observed after quenching whilst other failures occurred on handling. The cladding was oxidised on the outer surface by steam and on the inner surface by UO\(_2\). The nature and kinetics of oxidation of Zircaloy by UO\(_2\) have been characterised by Höffmann et al(66) and Parsons et al(67) and oxygen embrittlement of the inner surface is considered approximately equivalent to that occurring from steam oxidation on the outer surface. In-reactor equivalent isothermal film boiling temperatures were calculated using observed post-test phase thicknesses and an ideal diffusion oxidation code COBILD (see Section 5.4.2). The various oxidation related embrittlement criteria previously considered were determined and Haggag(65) compared the data obtained with the out-of-reactor embrittlement data obtained by Chung and
Kassner (44) for cladding quenched at a similar cooling rate i.e. \( \sim 100 \text{ Ks}^{-1} \).

Haggag concluded that the \( P_W < 0.5 \) and the ECR < 17% criteria poorly predict thermal shock failure and do not predict handling failure at all. The in-reactor thermal shock failure was predicted by the Chung and Kassner criterion of 0.1 mm of \( \beta \)-phase with < 0.9 \%o oxygen. The handling failures resulting from in-reactor tests were confined to two rods in which the cladding experienced film boiling in the breached condition and the handling failure of these rods was not predicted by any criterion. These rods were considered to be embrittled to a greater extent than intact rods oxidised for similar time temperature parameters. Hydride platelets were observed in the microstructure of these rods and it is suggested (68) that hydrogen influenced the fracture resistance. Chung (69) however points out that the calculation of the \( \beta \)-phase oxygen concentration gradient cannot be carried out very accurately for Zircaloy experiencing cycles of non-isothermal film boiling transients and thus comparisons are difficult. It was also noted that Pawel's criteria of 95% saturation and 0.7 \%o oxygen in \( \beta \)-phase accounted for all failures except the PBF handling failure and one ANL handling type failure but did not distinguish between the two phenomena. Haggag finally recommended that the Chung and Kassner criteria should be used for predicting failure either by thermal shock or handling for severe fuel damage experiments i.e. beyond design basis LOCA's and a comparison of the in-reactor and out-of-reactor data for the two embrittlement criteria are shown in Figs 25a and 25b.

5.4 CALCULATION OF TRANSIENT OXIDATION, HYDROGEN GENERATION AND EMBRITTLEMENT

The consequences of high temperature oxidation of cladding are hydrogen generation, heat of reaction and embrittlement of cladding. The hydrogen generated in a large break loss of coolant accident from the Zircaloy-steam reaction is, in the first few hours, the most significant quantity of hydrogen produced. In design basis accidents in which steam is unlimited, the hydrogen generated and the heat evolved can be calculated for transient oxidation by application of the isothermal parabolic rate constants for total extent of reaction. In steam limited conditions the calculation would be conservative.

The equivalent metal reacted may be calculated similarly to assess the embrittlement of the cladding with respect to the 17% ECR criterion. However, for the calculation of the detailed distribution of the oxygen in the \( \beta \)-phase for the assessment of embrittlement in terms of the Chung and Kassner criteria it is necessary to use more sophisticated techniques and more fundamental oxygen transport data. Transient oxidation may be computed using isothermal oxidation data by approximating the transient to a series of small isothermal steps, the sum of the isothermal oxidations representing the total oxidation of the transient.

5.4.1 Calculation of hydrogen generation

The hydrogen released when Zircaloy is oxidised in steam is directly proportional to the oxygen consumed or Zircaloy reacted. Thus for each mol of zirconium reacted two mols of hydrogen gas are produced, hence for each kilogram of zirconium reacted 0.044 kg or 0.491 m\(^3\) (STP) hydrogen are
produced and the amount of hydrogen produced at any time is expressed as:

$$W_{H_2} = 2 \cdot \frac{M_{H_2}}{M_{Zr}} (Kp \cdot t)^{\frac{1}{2}}$$

where $W_{H_2}$ = mass (mg) of $H_2$ produced per unit (cm$^2$) of Zr surface
$M_{H_2}$ = mol wt of $H_2$ (2.002) mg/mg mol
$M_{Zr}$ = mol wt of Zr (91.22) mg/mg mol
$t$ = time seconds
$Kp$ = parabolic rate constant (mg Zr/cm$^2$/s)$^{\frac{1}{2}}$

Thus the calculation of hydrogen generated as a function of time in a transient or the integrated hydrogen may be calculated using parabolic rates of reaction which are available over a very wide range of temperature ~ 700°C up to the melting point of Zr-4, as described in Section 5.2. The data are scattered due to difficulties in measuring temperature and different experimental and evaluation methods. Ocken (70) has suggested that external and internal heating of specimens results in parabolic rate constants with different pre-exponential factors and activation energies due to the different temperature gradients across the cladding wall. It is suggested that the correlation for internally heated specimens

$Kp = 3.33 \cdot 10^3 \cdot \exp - 33600/RT$ most closely represents the in-reactor fuel temperature gradients. The draft proposed ANS Standard for Containment Hydrogen Control (71) suggests use of the above correlation below 1205°C and the Baker-Just correlation above 1205°C. The latter correlation is shown in Figs 2-4 to be the conservative data. Overall, the data promotes an acceptable basis for the calculation of hydrogen generated provided the steam availability is maintained and that the oxidation does not deviate significantly from a parabolic rate.

The conditions of possible steam limitation have been described previously (Section 5.2.5). The deviation from parabolic oxidation has been observed by some workers at temperatures below ~ 1000°C but oxidation in this temperature range is low and the deviation is not considered significant for LOCA calculations. Pawel and Campbell (25) have pointed out that significant filling of the β-phase with oxygen influences the kinetics of oxide and α-phase growth but the overall effect is not significant for design basis LOCA calculations of total oxidation. The time for which the initial parabolic rate constants measured in the temperature range 750-1600°C for times up to 25 hours are applicable after including specimen geometry and breakaway oxidation effects, has been determined by Leistikow et al (21,22) e.g. Fig. 6. At very high temperatures, above the melting point of Zircaloy there is no reported data on the oxidation rate of liquid Zircaloy although work on this topic is reported to be in progress (72).

The calculation of oxidation or hydrogen generated using total oxidation parabolic rate constants has been assembled into computer codes examples of which are BILD5 (34) COBILD (65) and TRANS (9). Using the kinetic data of Pawel et al (13) a simple calculation has been performed by Sherman et al (73) to illustrate the rate of hydrogen generation from the oxidation of a Zircaloy surface area such as in the core of TM1-2, the time to generate 100 kg of hydrogen is shown in Table 4.
5.4.2 Calculation of oxygen distribution

The calculational techniques generally used to predict the distribution of oxygen in unstressed, isothermally or transiently oxidised cladding can be generalised into two types of computer code differing in the method of calculating the oxygen distribution in the oxide and alpha phases. In the first, simpler instance the position of the oxide/\(\alpha\) and \(\alpha/\beta\) boundaries are estimated by application of the experimentally determined parabolic growth rate constants for the oxide and alpha phases and hence only apply if semi-infinite parabolic oxidation is maintained. The oxygen concentration profile in the \(\beta\)-phase is calculated by considering the diffusion of oxygen through the \(\beta\)-phase using a finite difference technique. Examples of such codes are BILD5 and COBILD. The latter code is a development of BILD5 and also includes the calculation of oxidation on the inner surface by contact with \(\text{UO}_2\). For calculation of the critical \(\beta\)-thickness to survive thermal shock it is necessary to use methods in which the effects of saturation of the \(\alpha\) or \(\beta\) phases and deviation from parabolic behaviour, are accounted for.

More complex codes are also available which are based on finite difference solutions to the fundamental oxygen transport process, i.e. diffusion. Such codes also account in principle, for changes in the parabolic rates due to the approach to saturation. Examples of these codes include SIMTRAN/MULTRAN\((74,75)\) ZORO\((76)\) and PRECIP II\((77)\).

The basic data for these calculations are the isothermal oxygen diffusion coefficients in each phase and the equilibrium interface oxygen concentrations. The diffusion coefficients for oxygen in \(\alpha\) and \(\beta\) Zircaloy have been determined\((78,79,62)\) and for zirconia, deduced from oxidation experiments and previously established values of \(D_o\text{O}_x\) and \(D_{BOx}\). The interface solubilities for oxygen in zirconium\((80)\) and Zircaloy\((81)\) have also been published.

Calculations based on ideal models of diffusion assume that the diffusion coefficient is constant in material with an oxygen concentration gradient and that the concentration of oxygen at the phase boundaries is that of equilibrium conditions even in transient temperature time oxidation. These assumptions are not strictly valid and their use gives rise to erroneous predictions. Attempts have been made to replace, in the oxidation model, equilibrium oxygen concentrations at phase boundaries by empirically determined oxygen concentrations.

For a limited range of transients the codes predict oxygen concentration distribution with reasonable accuracy but for less simple transients certain aspects of the oxidation process are not accounted for by either a parabolic rate constant or an ideal diffusion model. Examples of departure from the ideal model are the formation of \(\alpha\)-incursions ahead of the \(\alpha\)-boundary\((25)\) and anomalous two-peak oxidation in certain transients with two temperature maxima\((9,82)\). In such transients the oxide is observed to be thinner on Zircaloy initially taken to a higher temperature. Pawel\((82)\) postulated that the effect was due to the hysteresis in the monoclinic/ tetragonal phase transformation over the temperature range 900–1200°C and concluded that insufficient data were available to permit modelling of the phenomenon although the conditions under which anomalous oxidation occurs are well defined.
The SIMTRAN code developed by ORNL and KfK\textsuperscript{(74)} solves simultaneously the diffusion and heat conduction equations in order to determine the oxygen and temperature profiles in the cladding wall. The most recent version of SIMTRAN contains material properties extending to 1800K\textsuperscript{(75)} and models the non-equilibrium concentration at the interfaces during transients and calculates the fuel temperature. The code has a particular application in beyond design basis accidents, i.e. severe accidents where the influence of oxidation exothermic heat induces large temperature gradients in the cladding and the influence on fuel behaviour is significant. In most design basis LOCAs the temperature gradients across the cladding are smaller and average temperatures are used for oxidation calculations. The MULTAN\textsuperscript{(75)} code was derived from SIMTRAN for design basis oxidation calculations by removing the heat transfer calculations. For further simplicity, cylindrical geometry is also replaced by slab geometry. Malang and Neitzel\textsuperscript{(75)} also suggest that the oxygen profile in β-phase after cooldown is best approximated by the profile existing just before fast cooldown. A limited amount of code validation has been published, cathcart et al\textsuperscript{(34)} concluded that both the early version of SIMTRAN and the BILDS codes adequately predict the thickness of oxides within 10% apart from the anomalous oxidation observed in certain two peak LOCAs. However if equilibrium oxygen solubilities are maintained, the α-phase thickness is overpredicted during rapid cooling. Malang and Schanz\textsuperscript{(83)} reported good agreement between oxide and alpha thicknesses calculated by a later version of SIMTRAN with a non-equilibrium model and experimental transient oxidations. Malang and Neitzel\textsuperscript{(75)} reported the use of the most recent version of SIMTRAN in predicting oxygen distribution in electrically heated fuel rod simulators, heated in simulations of severe accidents to ~2000°C. Future development of the code will include modelling of the heat and mass transfer between oxidised cladding and steam. This model will also address the concentration profiles of hydrogen and steam near the cladding surface to allow modelling of steam starvation and hydrogen blanketing.

Suzuki and Kawasaki\textsuperscript{(77)} have used the SIMTRAN code as a basis for improving and extending the prediction of oxidation parameters in LOCA conditions. The code PRECIP-II has been developed by improving the treatment of the boundary conditions during the cooling phase. In the code, as the temperature falls, the diffusivity of oxygen in the β-phase is reduced by a factor which is a function of the local supersaturation. Additionally, to obtain good correlation with measured oxidation parameters on transiently exposed samples, the temperature dependence of the oxygen solubility in the α-phase at the oxide-α boundary is modified, the diffusivity in the oxide and the β-phases are also modified slightly with respect to values previously used in the SIMTRAN code. Under these conditions the measured and calculated data for weight gain, oxide and alpha phases are within ±10% for Zircaloy exposed to transients similar to those postulated in a LOCA.

Biederman et al\textsuperscript{(9)} also made comparisons for the TRANS and ZORO codes and found reasonable agreement apart from the anomalous two peak LOCAs.

Transient oxidation of virgin and pre-oxidised Zircaloy has also been studied by Leistikow et al.\textsuperscript{(32)} Variables such as blowdown peak temperature, heating and cooling rates were considered. The authors
reported that there is a very variable oxidation response from pre-oxidised Zircaloy subsequently oxidised in transients. It is postulated that the detailed reactor operating history determines localised oxidation phenomena and that subsequent high temperature oxidation must necessarily reflect the varied starting conditions of low temperature pre-corroded oxide. At transient temperatures less than 1200°C preoxidation was generally protective but above 1200°C the subsequent oxidation was greater than that measured on initially unoxidised metal. The authors conclude that although high temperature transient oxidation on virgin metal is amenable to calculation it is difficult to define parameters which would enable acceptable calculations to be made for initially oxidised cladding in a wide range of transients.

A moving boundary model of oxidation has also been developed (84) at Chalk River and comparisons with experimental data are reported as satisfactory for both isothermal and transient heating.

At KfK the modelling of Zry-UO₂ interactions during high pressure transients is under way. The model PECLOX predicts the combined external and internal oxidation of the cladding due to the reaction with steam on the outside and with the UO₂ fuel on the inside surfaces. It describes the formation, growth, and disappearance of the various interaction layers and the corresponding oxygen profiles as functions of temperature and time. The model exists in a preliminary version. (195)

5.5 CONCLUSIONS

In a postulated loss of coolant accident, the Zircaloy-steam reaction is potentially the most significant source of hydrogen. However, if the ECCS functions according to the design basis, the hydrogen production from other sources such as radiolysis of water or containment metal corrosion reactions, generated over a longer post-LOCA time period would predominate.

Zircaloy oxidation, hydrogen and exothermic heat generation can be adequately calculated for transients typical of hypothetical design basis transients, using the available kinetic data on the isothermal oxidation of Zircaloy in unlimited steam. Such data are available over a wide range of temperature, from low temperatures ~700°C where oxidation rates are low, up to the melting point of Zircaloy. In the temperature range ~700-~1500°C there is some scatter in the data, generally accepted as being due to difficulties in accurate temperature measurement combined with differences in heating technique which produce different temperature gradients across the specimen thickness. However the Baker-Just correlation is conservative between ~1000-1500°C increasingly so as the temperature increases.

Other variables having a possible influence on the Zircaloy oxidation rate, such as steam pressure, preoxidation of cladding, deformation during oxidation, ionising radiation and anomalous oxidation from certain two peak transients are not well characterised but are not thought to have significant effects on calculations of hydrogen generated from the transient Zircaloy-steam reaction in postulated design basis loss of coolant accidents.
Above ~ 1500°C, temperature measurement is increasingly difficult and the tendency of zirconia to transform from the tetragonal to the cubic polymorph introduces additional uncertainty in measurements of oxidation rates. For calculations in hypothetical more severe accidents in which cladding temperatures may exceed ~1500°C, the data is sparse and scattered. Although the Baker-Just correlation is still conservative it has been shown to be possibly less so than at lower temperatures. Above the melting point of Zircaloy, there is as yet, no data on oxidation in steam.

If the availability of steam is limited, as postulated in some severe accidents, the hydrogen fraction of the steam may become significant. In high hydrogen/steam ratios the oxidation rate of Zircaloy has been shown to decrease. More data is required on the effects of hydrogen in steam on the oxidation and hydrogen pick-up of Zircaloy for the accurate modelling of the core heat-up at the high temperatures and in the limited steam conditions of more severe postulated accidents.

For the prediction of intact survival or fragmentation of fuel cladding in thermal shock due to re-wetting, the embrittlement criterion based on the total oxidation (equivalent cladding reacted) has been shown to be conservative, particularly at low maximum oxidation temperatures and also if significant wall thinning occurs. Embrittlement as defined by the total oxidation criterion can be calculated for design basis loss of coolant accidents by application of the available parabolic rate constants for the Zircaloy-steam reaction.

An embrittlement criterion based on the distribution of oxygen through the cladding wall has been proposed which adequately defines out-of-reactor and in-reactor embrittlement in thermal shock. A similar criterion also adequately defines resistance to ambient impact of 0.3 J, thought to be a reasonable approximation to post LOCA quench ambient impact loads. The criteria based on oxygen distribution are independent of the maximum oxidation temperature, the initial cladding thickness and the wall thinning resulting from ballooning deformation. Better definition of post LOCA quench impact forces likely to arise from reflood, handling, storage, transport and seismic events would improve confidence in the above criterion. Difficulties arise in the application of the suggested embrittlement criteria based on oxygen distribution from the present shortage of well validated calculational techniques for predicting oxygen distribution in cladding after transient oxidation. Transient oxidation in the realistic condition of fuel rod cladding oxidation in an in-reactor transient is influenced by the mechanical restraint imposed by the fuel stack and the build up of hydrogen in the bore of the cladding. These effects have been shown to result in deviations of oxidation rate from those measured on small unrestrained specimens in unlimited steam in laboratory conditions. (44)

Computer codes have been developed e.g. MULTRAN/SIMTRAN, using models of transient oxidation to calculate the distribution of oxygen, and the distribution of temperature in the cladding wall. The models are still being developed and further work is needed to improve the calculation of oxygen profiles in β-phase after rapid heating and cooling.
6. PLASTIC DEFORMATION OF CLADDING

6.1 The Problem

If the external coolant pressure falls to a low value the tensile stress which is produced in the cladding by the internal gas pressure is sufficient to cause plastic distension. The creep strength of Zircaloy falls rapidly with temperature so that at 700°C strain rates can be up to 1% s⁻¹. Furthermore, the ductility of Zircaloy is high so that strains of 50% or more are possible in principle. The spacing of PWR rods is such that adjacent rods straining by 32% will touch; the key question is thus whether strains of this magnitude or greater can occur in practice in adjacent rods at the same level in the assembly. The associated question of how coolability is affected by gross strain leading to progressive blockage of the coolant sub-channels is not addressed here.

Internal pressures of fuel rods

The fuel rod is internally pressurised with helium during manufacture, and this pressure is augmented during irradiation by the release of gaseous fission products. The helium is added to improve thermal conductivity at the pellet-cladding interface, (85) and to reduce mechanical interaction between pellet and cladding during service. (86) A usual filling pressure is of the order of 2 MPa; this is increased by the higher temperature during operation and further by fission product release. The temperature transient occurring in an accident also increases the pressure, but there should be little further release of fission products. (87) Any distension of the cladding reduces the internal pressure by increasing the free space volume.

6.2 Factors controlling deformation and rupture of cladding

The basic parameters controlling deformation are stress, temperature and creep strength, the latter being affected by oxidation, grain size and anisotropy. When the temperature of a stressed tube is uniform, deformation is unstable, i.e. if the tube increases in diameter at any one axial position, the increased stress resulting from the larger diameter and reduced wall thickness will lead, by positive feedback, to a runaway deformation to rupture. In an assembly of rods at a uniform temperature, these local ruptures would occur adventitiously depending on local variations in materials properties or dimensions, and blockage would be highly improbable. In a cooled environment, however, it is possible for a thermal stabilising process to lead to extended deformation, (88) - if a local region of cladding is swelling preferentially, it will be cooled to a greater extent than neighbouring regions, reducing its creep rate relative to theirs. This negative feedback effect has been found (89) to operate over a range of heat transfer conditions for tubing which is heated ohmically so that the power is generated directly in the cladding. When the heat source is internal to the cladding, as in an actual fuel rod, a further negative feedback effect is possible, from the thermal resistance interposed between pellet stack and cladding by the gas gap opened by distension. The conditions of external heat transfer obtaining in a fuel assembly following a LOCA, with a large component of convective cooling by uprising steam, will tend to produce axial temperature gradients in the fuel rods, so that distension, if it occurs,
will vary along the rods. Adjacent rods, however, will tend to deform similarly though not identically.

A temperature transient which produces significant strains is quite likely to strain the cladding to rupture. This will check distension by releasing the pressure differential stressing the cladding. Since the inherent ductility of Zircaloy is high, early rupture, at low overall strain, will occur only when the ductility of the material is exhausted locally by non-uniform straining. A prime cause of non-uniformity is circumferential variation in temperature around the cladding, since the creep-strength of Zircaloy is highly temperature-sensitive, and the cladding will strain preferentially in the hottest region. Such non-uniformity is caused, for example, by the fuel pellet stack not remaining co-axial with the cladding as the latter distends.

Apart from non-uniformity in heat transfer, either internally from an eccentric pellet stack or externally from, for example, inhomogeneous coolant flow, a further mechanism for introducing circumferential temperature variation in a rod follows from the anisotropic properties of the cladding. Fuel cladding manufacture imposes large reductions in wall thickness to avoid the formation of radially aligned hydride platelets during service; this results in in the (0001) poles being aligned close to the radial direction with a texture that resists wall thinning under the biaxial tension generated by an internal pressure. When the cladding distends, the deformation tends to be accommodated by material flowing axially into the region of distension. This results in axial contraction, particularly noticeable in axially unrestrained burst tests. If wall thinning occurs preferentially at one side of a tube as a result of a local temperature increase, caused for example by contact with an internal heater rod or a fuel pellet, then only that side will shorten and the tube will bow against the heat source, Fig. 26. This ensures that deformation continues at one side - the "hot-side straight" effect - and since only a part of the circumference is thinning the total circumferential strain potential of the tube is not achieved. Direct evidence for this has been produced at KfK by a cine film of X-ray images from a specimen and its heater during bulging. Strain anisotropy is at a maximum in the range 725-775°C, but declines rapidly in the two-phase region.

If the combined radial strains of neighbouring rods exceed 65% they will touch. After touching, the radii of curvature of the sections of cladding not in contact will decrease, reducing the hoop stresses in them. Deformation may continue above and below the contacting region; there the deformation would continue as before only if the temperature rose. Time to rupture is likely to be longer when mechanical interaction occurs.

Fuel cladding under normal service conditions is in the cold-worked and stress-relieved condition, the structure consisting of distorted lenticular grains. This structure recrystallises above 700°C in the order of 10 s. Thus following a LOCA the defects introduced by irradiation would be removed as the boundaries of the recrystallising structure swept through the lattice. The migrating boundaries would also prevent the build-up of stresses during deformation, suppressing crack formation. This effect, coupled with the fact that the texture resists wall thinning,
and so would produce a diffuse neck, leads to the possibility of high circumferential strains during deformation in the range 700–800°C.

Above about 800–820°C the transformation to the beta-phase begins. The presence of beta-phase in the alpha-matrix reduces the strength considerably, while the amount of beta is very temperature-dependent. The net effect is that in the mixed-phase region the influence of circumferential temperature variation in reducing the total strain at rupture is greater than in the single-phase region. Ductility remains high, but large strains do not usually occur unless strain rates are low.

6.3 Experimental data

Relevant experiments may be divided into the classes:

(a) Materials properties tests to determine creep strength, effects of anisotropy etc. These may or may not utilise actual cladding specimens.

(b) Determination of oxidation and embrittlement. These have been described in a previous section.

(c) Determination of deformation under internal pressure.

The last class may be divided into single-rod and multi-rod tests, by type of heating (ohmic, internal, furnace, or nuclear heating) and according to the realism of their external heat transfer conditions. Single-rod tests performed out-of-reactor form the bulk of published results(93) and these have largely served to elucidate the controlling factors described in the preceding section. The actual strains found in single-rod tests, however, cannot be taken as representative of those occurring in an assembly, since the effects of neighbouring rods, direct or indirect, are not present. The main series of multi-rod out-of-reactor tests have been at ORNL,(96) KfK(93) and JAERI.(97) The ORNL tests on 4 x 4 and 8 x 8 assemblies were performed with conservatively low steam flows, and consequently very low heat transfer coefficients, to provide data for evaluating specific requirements for the US Federal Regulation 10 CFR Part 50 Appendix K. (An analysis by the Nuclear Regulatory Commission of the cladding deformation question is presented in ref. 200.) The test conditions were conducive to significant deformation and were selected to provide a reasonable estimate of the upper limit of the amount of deformation that could be expected under Appendix K conditions. In addition, the tests point out the differences between the behaviour of rods singly and in assemblies and between small and large assemblies. The tests cannot however be used directly to infer fuel assembly behaviour in accidents with reflood. A similar comment applies to the tests on 7 x 7 assemblies at JAERI. The tests in the REBEKA series at KfK have been the most realistic performed to date out-of-reactor, with simulated bottom reflood. Significant co-planar strains, but acceptable restriction of sub-channels, have been produced.

In-reactor, tests have been performed on single rods in West Germany in the FR-2 reactor and in the USA in PBF. Extended deformation has occurred in both these series.
The only sources of multi-rod in-reactor data are the series of experiments in the NRU reactor at AECL Chalk River (98) and the PHEBUS series at Cadarache (99-102). The current series of tests in the Halden BWR Flow Starvation Rig (103) will also fall into this category.

The experimental programmes referred to are discussed in more detail in the rest of this section.

6.3.1 In-reactor tests

6.3.1.1 Single rod tests in the FR2 reactor

A series of in-reactor experiments using PWR-type single rods (active fuel length 50 cm, enrichment 4.7%) both unirradiated and irradiated (2500-35000 MWD/t) and simulating the second heat-up phase of a cold leg break has been carried out in the DK loop of the FR-2 reactor at Kernforschungszentrum Karlsruhe (KFK) West Germany. Also for comparison, tests were carried out in the in-reactor loop under identical conditions except that internal electrically heated fuel rod simulators were used. The test programme is summarised in Table 5. (87)

The main parameter was burn-up which was achieved in the FR-2 reactor with fuel centre temperatures of 100-250K lower than in commercial PWR fuel and a coolant pressure of only 3 bar. The rating was slightly above the peak rating of a commercial PWR. The secondary parameter was the rod internal pressure which ranged from 25-125 bar. The desired initial pressure for each test was produced by additional pressurising with helium, just prior to the transient testing, to augment, where necessary, the fission gas generated during the previous irradiation, but there was no pressure control during testing.

The transients which the rods experienced were initiated by interrupting the loop coolant flow of superheated steam at 573K, 60 bar, and simultaneously depressurising the test section of the loop. The test rod power was kept constant at about 40 W/cm until the cladding temperature reached approximately 1200K, then reduced by reactor scram. The procedure resulted in heat-up rates in the range 6-20K/s and rupture of all the pressurised rods during the heat-up phase. A typical transient test is shown in Fig. 27. The effect of 'cladding lift-off' can be seen in thermocouples 131 and 132 which were in the region of major deformation. These show a pronounced drop in temperature as the size of the fuel cladding gap increases as indicated by the fall in pressure.

The burst temperatures quoted are considered to be those at the burst location at the time of burst and were obtained by extrapolation from the nearest cladding thermocouple reading to the burst.

The burst pressure was that measured at the start of the fast pressure drop signifying rupture. The pertinent time after initiation of the transient is called the burst time.

The pressurised rods exhibited circumferential straining over their entire heated length. The axial deformation profile was in general influenced by the power profile and locally by thermocouples, the maximum strain and burst occurring at or near the peak power position.
Figure 28 shows the strain profiles for unirradiated rods (A,B) and for irradiated rods (C-G).

In test E5 the reactor was scrammed at the onset of ballooning in contrast with the other tests so that the cladding temperature decreased whilst the major part of the deformation was occurring. This rod deformed to the limit of 67% where it contacted the shroud over an axial length of 10 cm. Presumably the trapping of the bulge by the shroud caused the deformation to extend axially. The post-test neutron radiograph is shown in Fig. 29.

The burst strains of irradiated, unirradiated and electrically heated rods are shown in Fig. 30. There was no influence of irradiation damage, iodine or other fission products on the deformation and rupture. However, low ductility failures due to iodine stress-corrosion cracking are not likely at temperatures above 700°C even if it is assumed that there is complete release of iodine from the high burn-up fuel. Also the effects of irradiation damage on the cladding are annealed when the temperature exceeds 700°C.

There was essentially no additional cracking of the irradiated fuel pellets during the transient tests. The mean fragment size was about 3 mm. Only a small additional amount of fission gas was released during testing.

Neutron radiographs of the rods after testing showed that in the regions of major deformation the UO₂ fuel pellets had completely fragmented and the fragments relocated outward and downward filling the space in the fuel rod created by the diametral expansion of the cladding. There are indications from three thermocouples placed in the upper end region of the fuel stack that collapse of the fuel column occurred at the time of burst, since the temperature monitored by these fell dramatically at burst whilst others mounted lower down the fuel rod behaved normally, see Fig. 31.

6.3.1.2 Single rod tests in the PBF

The LOC series of large break LOCA simulation experiments has been carried out in the Power Burst Facility (PBF) at the Idaho National Engineering Laboratory (NEL) in the USA.

The test programme was designed to investigate the two major parameters determining cladding circumferential strain during ballooning, i.e. cladding temperature and differential pressure, using both unirradiated and irradiated rods. In each PBF LOCA test there were four separated 15 x 15 type PWR test rods 0.91 m long, two of which were internally pressurised to values representative of PWR rods at start of life and two which were pressurised to values representative of high burn-up rods. The plenum volume was scaled in proportion to the active length compared to that of a full length rod. One of each type of pressurised rod had been previously irradiated in the Saxton PWR, under conditions typical of commercial power reactors. The fuel rod and shroud design along with details of the instrumentation and test system are given in Reference 105.
Peak cladding temperatures of 1070, 1190 and 1350K were planned to be attained in the tests by varying the rod power history and time of critical heat flux (CHF). These temperatures correspond respectively to the region where maximum ductility occurs in alpha-phase Zircaloy, the strain-rate-dependent ductility trough in the transition region from alpha to beta phase, and the region of maximum ductility in the beta phase under oxidising conditions.

Each test with four single rods consisted of a pre-blowdown power calibration and decay heat build-up, blowdown (depressurisation of the coolant) and test termination by rapid quenching. During depressurisation the normally sub-cooled water surrounding each test rod flashed to steam as the coolant was released through the cold leg and the cladding temperature increased.

The axial power profile along the fuel rods was shaped to flatten the power in the centre third of the active fuel length to simulate conditions typical of the central region of a PWR core. The reactor power was controlled during blowdown by the PBF transient rods and programmed to follow the power function, determined in pre-test calculations, necessary for additional heating of the test rods so that they might reach and maintain the desired test temperatures.

Out of the thirteen rods tested in the LOC series, nine ballooned and ruptured, two did not burst and two failed to achieve the planned transient owing to malfunction of the equipment. The thermal and mechanical behaviour data for the eleven rods which were satisfactorily tested are given in Table 6.

The cladding strain was generally concentrated within the axial zone of uniform temperature, i.e. between 0.2 and 0.5 m from the bottom of the fuel stack and failure generally occurred between 0.2 and 0.4 m.

The circumferential strain at the burst elevation and over the axial zone of uniform temperature was greater in the rods previously irradiated in Saxton PWR than in the unirradiated rods for both tests which burst in the high alpha region (~1070K) and the alpha plus beta region (~1115K) see Figs 32 and 33.

Metallographic examination showed that the wall thinning was primarily concentrated in the burst region of the cladding of the unirradiated rod, whereas in the case of the previously irradiated rod the wall thinning was more uniformly distributed around the circumference.

This difference is likely to be associated with a more uniform temperature around the circumference as the cladding creep is very dependent on temperature. This encouraged uniform "lift-off" of the cladding from the fuel, thus avoiding or substantially delaying operation of the 'hot-side straight' effect which limits circumferential strain. A possible cause of more uniform temperature around the circumference is more intimate contact between the cladding of the previously power-reactor-irradiated rods and the fuel compared with fresh fuel rods where the pellet-clad gap can vary around a pellet and along the rod. Further supporting evidence for this effect can be obtained by comparing the deformation of Rods 7A and 7B. Since fresh rod 7B suffered extensive
cladding collapse onto the fuel stack during test LOC-5B and then was ballooned and ruptured during test LOC-5C, whilst the identical fresh rod 7A ballooned and ruptured during LOC-5A which had nearly identical thermal hydraulic conditions with test LOC-5C, the only difference should be the effect of a more circumferentially uniform gap conductance.

A comparison of the axial strain profiles of rods 7A and B is given in Fig. 34. Rod 7A, which was undeformed prior to blowdown, burst with a circumferential strain of 19% whereas the previously collapsed cladding of Rod 7B ruptured with a strain of 48%, although the maximum strain (about 56%) was found elsewhere on the rod. It is suggested that sufficient negative feedback effects(89) halted the strain at the maximum region and that the changing thermal hydraulics then shifted the maximum temperature region above 0.2 m where failure eventually occurred.

6.3.1.3 Single rod tests in the ESSOR reactor

Six tests were performed in the EOLo-JR facility in the ESSOR reactor Ispra, Italy to study the ballooning behaviour of PWR-KWU type fresh fuel rods in a simulated LOCA transient.(106)

The test specimens contained a 1 m long stack of natural UO₂ pellets 9.10 mm o.d. inside Zircaloy-4 cladding (10.75 mm o.d. 9.3 mm i.d.). They were pressurised cold to about 4.8 MPa via a gas line to the rod plenum. A pressure transducer which was outside the reactor core was connected into this line to record the change in pressure during the test and indicate rupture.

The external heat transfer medium was a helium + 5% oxygen mixture(107) and simulated spacer grids were placed at 0.25 and 0.75 m elevations.

The tests were carried out by adjusting the reactor until the rod developed the prescribed power the clad temperature being controlled at 820K by adjusting the gas circulation. This temperature was held for about two hours to allow detailed measurements of the initial state of the rod. After these had been made the coolant flow was reduced to allow the test rod to increase in temperature at about 3 K/s to the desired value in the range 970-1080K for the particular test. This temperature was maintained by control of the gas flow until the cladding had deformed and ruptured. The reactor was then scrammed to cool the specimen. This sequence is shown diagrammatically in Fig. 35.

The first test was devoted to calibrating the instrumentation. In the subsequent five tests the rods bulged and ruptured at five different control temperatures in the high alpha phase region (997-1080K).

Details of the main measurements made during these tests are given in Table 7 and in Fig. 36 the axial strain profiles, azimuthal temperature measurements and position of bursts are given. The latter occurred very close to the measured maximum temperature in all cases.

The rod pressure and clad temperature during the test EOLo 2 are shown in Fig. 37 and the clad temperature and neutron flux profiles in Fig. 38. In four of the tests the burst occurred at the elevation 0.65 m
which is close to the estimated position of the peak clad temperature, but
in EOL0 3 the burst elevation was 0.45 m. This test had the highest clad
temperature and at the end of the temperature ramp the coolant flow had to
be reduced to zero to achieve this. The effect was to displace the peak
temperature elevation.

The results showed that significant axial and circumferential
temperature gradients developed during the test indicating the operation
of the stabilising strain-cooling effect(89) and the 'hot side straight'
effect, which tends to keep clad in contact with the fuel at one side.

6.3.1.4 Single rod tests in the SILOE reactor, Grenoble(202)

Experiments have been carried out in the FLASH loop in the SILOE
reactor, using single 300 mm long rods. The primary aim of the programme
is to study fission product release during a LOCA, but data have also been
obtained on cladding deformation and oxidation and on fuel stack
behaviour. The loop is blown down with residual fission power simulating
decay heating, and is then reflooded. Peak strains of 18-62% have been
recorded. These data have been applied to validation of the CUPIDON
code.(169)

6.3.1.5 Multi-rod tests in the NRU reactor(98,108-110)

Multi-rod tests in a test loop of the NRU reactor at Chalk River,
Canada, have been carried out on behalf of the US NRC and the UKAEA.
These tests use full length PWR fuel rods of the 17 x 17 type, which are
about two feet longer than the core of the reactor so that the bottom one
and a half intergrid lengths and the top one half of an intergrid length
are not in the neutron flux, see Fig. 39. In tests MT-1 to 4 a maximum of
twelve rods were pressurised so that they could bulge and rupture. These
are arranged in a cruciform shape and surrounded by guard rods which do
not deform, see Fig. 40. These guard rods are spaced at the standard
pitch of the pressurised rods so that the latter need to strain over 60%
before there is sufficient contact with the guard rods for the bulges to
be restrained.

The tests were carried out using fresh UO2 fuel 'conditioned' by
briefly raising to full power three times just prior to the experiment.
Post irradiation examination(108) has shown that this treatment cracked
the fuel pellets in a similar manner to fuel in a power reactor; however
no other irradiation effects can be simulated by this treatment. In the
tests proper the reactor power was raised to a level equivalent to decay
heat and the assembly cooled by flowing steam. The transient was
initiated by shutting off the steam flow and allowing the assembly to heat
up under adiabatic conditions achieving a temperature increase of about
8K/s.

The transient may then be terminated by tripping the reactor.
Alternatively two-phase cooling conditions can be created by admitting
water to the bottom of the test section. This reflooding water produces
enhanced cooling which checks the temperature ramp, producing a flat-
topped transient, and ultimately quenches the assembly.
In three of the tests, MT-1, 2 and 4, the deformation occurred during the ramp part of the transient in steam whereas in MT-3 the major deformation occurred during the flat-topped portion of the transient under two-phase cooling conditions. The distribution of strain along the rods in the MT series is shown in Figs 41a and 41b. It is evident that substantial co-planar deformations were produced and that the grids had a major influence in restraining the deformation locally. In MT-3 the grids also had a significant effect on the axial shape of the balloons. This results from de-superheating of the steam by water droplets when the two-phase coolant experiences turbulence as it encounters and passes through the grid structures. Thus the onset of significant strain occurs in the top end of the intergrid space and is terminated by the downstream grid, see Fig. 41b, producing the characteristic 'carrot' shaped balloon.

In MT-1, 2 and particularly MT-4 there were two peaks in the deformation in between grids. The reason for this is unknown.

In MT-2 the ruptures occurred early in the transient. In this test it is likely that the fragmented fuel slumped into the bulged regions when the rods suffered the shock of bursting as in the case of the FR-2 tests described in 6.3.1.1. Thus during the remainder of the transient where two-phase flow conditions existed for 80 s, whilst the ruptured regions were above 1050K,(109) the conditions existed for generating local excessive cladding temperatures by increased volume of fuel in the ballooned regions as suggested in an analytical study by Bergquist.(111) This mechanism has also been reviewed by Broughton et al.(204) However there was no evidence of excessive temperatures being achieved in those regions. The surfaces of the ballooned regions appeared similar to the other parts of the rods, and no evidence of embrittlement emerged during PIE.

Post irradiation examination of the rods from the MT series 1-4 showed that except for the actual burst positions or where they were dented by the impact of adjacent bursts, the bulges remained circular.(110) This indicates that the conditions for mechanical restraint from rod to rod interaction as seen in tests with a greater number of pressurised rods, (see Sections 6.3.2.3, 6.3.2.4 and 6.3.2.7) were not present.

Haste(112) has modelled the effect of there being more pressurised rods and deduced that their presence would result in a reduction of the 'total' coolant flow area from 55% as measured to 70%. These calculations do not include any effect of clad denting owing to non-simultaneous bursts.

A further test MT-6A has been carried out with all the 21 rods of a 5 x 5 array minus the corner rods, pressurised. All the rods ruptured during the ramp part of the transient in near adiabatic conditions. Preliminary examination of the bundle has been carried out by cutting holes in the shroud. This showed that all the ruptures occurred in the inward looking direction and complete rod-to-rod interaction was observed at one elevation.

6.3.1.6 Multi-rod tests in the PHEBUS loop Cadarache, France

The CEA is carrying out a programme in the PHEBUS loop, which is installed a pool type reactor, to study the behaviour of 5 x 5 fuel rod bundles in a simulated PWR large break LOCA.(99-101) The rods have an
active length of 800 mm and are of the 17 x 17 PWR type, (see Fig. 42). The first series consisting of eight thermal hydraulic experiments using unpressurised rods and one burst test *(102)* was started in 1980 and completed in 1982. These experiments were carried out with the fuel rod bundle contained within an unheated shroud. In the burst test *(215P)* twenty-one of the rods were initially pressurised to 4 MPa to cause them to burst in the high alpha phase region, the remainder i.e. rods 2, 4, 6 and 16 did not deform owing to their low internal pressure. The plenum volume was 3.2 cm².

The pressurised rods burst within 14 s of one another at the top of the ramp position of the transient at about 1110K in steam, just prior to reflooding, see Fig. 43. The scatter in burst times is considered to have resulted from non-uniform rewetting which affected clad deformation by emphasising circumferential temperature gradients *(102)*.

Examination of the bundle showed the clad deformations to be carrot shaped and co-planar, see Figs 44 and 45 with strains greater than 33% over about 80 mm in those rods in the central 3 x 2 array which had not been affected by the non-uniform re-wetting i.e. 7, 8, 9, 12, 13 and 14.

The strains in the peripheral rods were appreciably lower, 15-35%, compared with those in the central 3 x 2 array, 43-53%. This is an expected consequence arising from the steep circumferential temperature gradients induced by the unheated shroud *(102)*. Nine of the eleven pressurised rods had strains greater than or equal to the maximum strain of 24% seen in the three central rods 17, 18 and 19 affected by non-uniform rewetting.

The four tie rods, see Fig. 46, provided sufficient mechanical restraint to induce the balloons in the 3 x 2 array to interact and the central rod adopted an almost square shape i.e. similar to those seen in out of reactor tests where there had been restraint *(126-132)* Fig. 107. The average flow restriction for the central 3 x 3 array i.e. including the three rods with low strains was 65% *(102)*. However the greatest individual flow channel reduction was approximately 80%.

6.3.1.7 Multi-rod tests in the Halden Reactor *(103)*

The OECD project is carrying out a series of experiments (IFA 54X) in a test loop in the Halden Boiling Water Reactor to compare the deformation behaviour of nuclear and electrically heated bundles under nominally identical conditions simulating a LOCA. The test assembly consists of a central pressurised rod surrounded by four pressurised rods and four guard tubes representing rods already ballooned to 30%. These tubes also carry the reflooding water.

6.3.2 Out-of-reactor tests

6.3.2.1 The REBEKA programme at Kernforschungszentrum Karlsruhe (KfK) W Germany

This programme *(90)* uses single and multi-rod geometries to investigate the deformation of Zircaloy fuel cladding during the refilling and reflooding phases of a LOCA, see Fig. 47. There is particular
emphasis on the conditions relating to the KWU design of PWR which includes hot leg injection of cooling water in addition to the cold leg injection used in American designed PWRs. The fuel rod has a plenum at both top and bottom instead of one at the top as in American designs. There is thus more than double the internal volume in the German rod, i.e. ~ 40 cc. (113)

The source of heat in REBEKA is an internal electrically heated fuel simulator shown in Fig. 48. (114) The multi-rod tests use full-length rods (heated length 3900 mm) with spacer grids at positions corresponding to those in a fuel assembly. The test loop can provide steam cooling from the top to simulate hot leg injection effects, and steam or reflooding water from the bottom. These facilities enable the rig to simulate the adiabatic heating of a 'blown-down' core followed by emergency cooling, (see Fig. 49).

The REBEKA test philosophy and objectives (115, 116) are given in Table 8. Many single-rod tests have been carried out from which two important features of deformation have been identified. The first is the so-called 'hot-side straight' effect. The second feature is direct experimental evidence of the influence of azimuthal temperature variations on the total circumferential strain (see Fig. 50). The test series has also yielded extensive knowledge of the effects of temperature and heating rate on circumferential burst strain from 970 to 1400K, (see Fig. 51).

Four multi-rod tests R1 to R4 with hypothetical reactor LOCA conditions, have been carried out using a 3 x 3 array of deforming rods surrounded at the same rod pitch by an outer row of unpressurised guard heaters clad with Inconel. Thus the outer row of deforming rods has to strain about 65% before bulges can be restrained from bursting by being trapped by two or more rods. The possibility of this particular aspect of multi-rod behaviour was investigated in a materials test (117) in stagnant steam and under reflooding conditions in R5 and R6 which utilised arrays of 7 x 7 pressurised rods, (see Table 8), restrained by a shroud placed at one half a rod pitch from the centres of the outer rods. The conditions for the first four tests were peak rod power 20 W/cm, internal pressure 70 bar, system pressure 4.5 bar cold flooding rate ~ 3 cm/s and heat up rate during the initial temperature ramp about 7 K/s. The materials test was carried out with reduced power to the rods.

The test procedure is firstly to circulate water around the lower part of the loop and steam down the test section; both exit via the same valve. Meanwhile the test bundle is heated to about 800K and the rods are pressurised. Then they are fed with 8.57 kW each which causes them to ramp in the downward flowing steam at about 7 K/s. Reflooding is initiated by shutting the exit valve which forces the circulating water to flow up the test section. This firstly reverses the flow of steam and then, as the water contacts the hot rods, produces an upward flow of steam with entrained water droplets i.e. two-phase cooling. During reflooding the power is reduced to 6.6 kW ± 3% to give 20 W/cm, (118) equivalent to decay heat, in the central region of the fuel rod simulators. The reason for the extra power during heat-up is that the rods have about 20% greater heat capacity than an actual fuel rod and thus require the extra power ramp at the rate calculated for actual fuel rods.
Tests R1(93) R3(90), R4(115,116) and R5(119) represent the behaviour of medium-rated assemblies in the KWU PWR, which for a LOCA are calculated to reach 930-950K by the time the reflooding water contacts the bottom of their heated length, whilst test R2 represents the behaviour of a highly rated assembly which is calculated to have reached 1120K at this time.

R-5 was the first test with a 7 x 7 array, all rods pressurised. The reflooding rate and rod powers were similar to those used in R-3 but the internal pressure was higher.

The R-6 test(120) had the objective of examining the influence of coolant flow direction on the deformation thus no reversal of flow direction occurred between the single and two-phase flow regimes, unlike the previous tests, R1 to R5. This test was selected for analysis using computer models, as a German Standard Problem (DSP7) and simultaneously as an International Standard Problem (ISPI4).

In R1(93) there was a small malfunction which caused an additional slug of cooling water to be introduced at the start of reflooding; this caused the initial temperature drop, see Fig. 52. However, the cladding which had started to deform in the steam phase at the top of the ramp continued to do so for about 100 s until rupture occurred during the period of two-phase flow. The combination of the increased surface area owing to clad deformation and the external cooling balanced the heat input so that the clad temperature remained reasonably constant for over 100 s producing a 'flat-topped' transient, see Fig. 52. The maximum cladding strain was 32% and the maximum coolant channel blockage ratio 25%. The position of the maximum strain in the rods tended to be between the centre of the inter-grid spacing and the next higher grid, i.e. shifted from the centre in the direction of coolant flow, (see Fig. 53). This is a consequence of two phase flow in the presence of spacer grids.

Thermodynamic non-equilibrium results in steam temperatures up to about 870K within the two-phase mixture of superheated steam and saturated water droplets. The turbulence and the break-up of water droplets induced by the spacer grids results in desuperheating of the steam and better heat transfer which lowers the temperature of the cladding downstream of each spacer. But superheating redevelops as the mixture flows along to the next spacer producing an axial temperature gradient of up to 50K between two spacer grids at the time of burst, (see Fig. 54). This results in an axial shift of the maximum strains in the rods towards the upstream spacer and enhances the formation of co-planar deformation, while limiting its axial extent.

In test R-2(114) the reflooding was delayed to allow the rods to reach 1120K and in this case all the deformation occurred rapidly during the heat up phase in superheated steam which was flowing down the test section simulating the effect of hot leg injection, see Fig. 57. The deformation was shifted in the direction of the steam coolant flow and large deformation occurred over two intergrid spaces, see Fig. 53. The bulges tended to be more coplanar than those produced during two-phase flow i.e. R1, R3, R4. (Compare Figs 53 and 55). The maximum circumferential strain was 64% and the maximum flow blockage 60%.
R-3(90) was essentially a repeat of R-1 but avoiding the malfunction which produced the additional cooling. The overall results were similar but the deformation was greater; the strains in two rods exceeded 60% and the maximum flow blockage was 52% (see Table 9).

R-4(115,116) was a similar reflooding experiment to R-3 but one of the pressurised rods was replaced by a (cooled) simulated control rod guide tube to investigate if the presence of such a tube would result in lower deformation in the multi-rod array. This rod did cause pronounced azimuthal temperature differences in adjacent rods but surprisingly the deformation of the array was not only similar to that of R-3 but maximum strain was greater than in R-3, where there had been identical thermalhydraulic conditions but no 'control rod guide tube'. The maximum circumferential strain was 79% and the flow blockage 55%. The deformation and blockage in R-3 and R-4 are compared in Fig. 55. The ruptures in R-3 were axially distributed, see Fig. 56, and a similar distribution was seen in R-4.

The cladding deformation characteristics in R-5 were similar to those in R-3 i.e. the regions of maximum strain were axially distributed over the intergrid region, see Fig. 58. Thus although the circumferential strains tended to be higher in R-5 the maximum blockage in both R-3 and R-5 was 52%. The axial distribution of maximum strains was sufficiently spread so that significant rod to rod interaction did not occur even though the potential for such interaction was increased by virtue of the larger number of pressurised rods, see Fig. 59.

In R-6 the rods were pressurised to 60 bar whilst steam at 150°C flowed through the rig. Then a power of about 7.8 kW was applied to each fuel rod simulator causing the bundle to ramp at about 7°C/s to 765°C. The power was reduced to 6.6 kW at this time and reflooding (3cm/s cold reflooding rate) was initiated.

The maximum internal pressure in the inner 18 rods which behaved in a satisfactory manner was 76.6 bar. The first burst in this group of rods occurred 20.5 s after reflood was initiated and the last after 37.7 s. Their axial strain profiles, see Fig. 60, tend to be coplanar and the regions of maximum strain were shifted in the direction of two-phase coolant flow i.e. away from the downstream grid as in the MT-3 test, Fig. 41c. The burst strains ranged from about 25 to 65%. The maximum channel blockage of 60% was very localised, but 50% blockage extended for 10 cm, see Fig. 61.

The axial strain profiles in the central intergrid span of tests R1-R6 are shown for comparison purposes in Fig. 62 which clearly demonstrates the influence that coolant flow direction and changes in direction during deformation can have on the rod strain profiles.

The main features of the five 3 x 3 REBEKA tests, the 7 x 7 materials test R-5, and the Standard Problem Test R-6 are given in Table 9.

6.3.2.2 Single and multi-rod testing at KWU, Erlangen, W Germany

The KWU (Kraftwerk Union) programme(121-125) consists of three major phases:
1. Cladding tube material behaviour

2. Fuel rod behaviour in single rod geometry

3. Fuel rod behaviour in multi-rod geometry

The test parameters used for studying the above are shown in Figs 63 and 64 and in Table 13.

1. The material behaviour work compared KWU standard cold worked and stress relieved cladding with cladding modified as shown below:

(a) Recrystallised: 2 h at 880K (610°C) in vacuum

(b) β-quinched from 1320K (1050°C) at 50-60K/s

(c) Oxidised: layers up to about 40 microns produced by heating at 670-690K (400-420°C) for up to 9000 h.

(d) Varying wall thickness.

The results of the above investigation are shown in Figs 65, 66 and 67.

The stress and temperature dependency of the strain rate of the cladding tube diameter was determined for the KWU cold-worked and stress-relieved cladding. Experimental values of the stress exponent n and the apparent activation energy Q in the Norton creep equation were obtained.\(^{(124)}\)

2. The aspects of fuel rod behaviour in single rod geometry which were examined were:

(a) Influence of azimuthal temperature variation on burst strain. Results (see Fig. 68) were similar to those of other workers, (KfK, SNL). The degree of tube bending resulting from azimuthal temperature is plotted in Fig. 69.

(b) Tube bending which is a characteristic feature of Zircaloy cladding deformation in the alpha-phase region. In this study the test specimens were standard KWU cladding. They were directly heated to 800°C and caused to deform by internal gas pressure firstly without and secondly with the influence of imposed circumferential temperature differences produced by an external heating and cooling device. The upper and lower ends of the tube were restrained from moving laterally whilst the lower end was free from axial restraint.

The first type of experiments showed that axial contraction increased with increasing tangential strain, see Fig. 70, confirming the findings of similar studies. In the second type of tests the average axial contraction in the ballooned region was measured simultaneously on the hot and cold side of the cladding and preferential contraction on the hot side was found, see Fig. 71.
The amount of tube bending was evaluated from the ruptured samples for those tests with a non homogeneous azimuthal temperature distribution. Tube bending increases rapidly with increasing azimuthal temperature difference, see Fig. 72.

(c) Influence of steam cooling on ductility and axial strain profile. No significant deviation could be found when the results of tests in steam were compared with those produced in air. However the steam flow in the test section shifted the maximum strain region in the flow direction, see Fig. 73 as has been seen by other workers.

3. The multi-rod tests were carried out to 'investigate whether the results of single rod tests are influenced by the geometrical and thermal conditions in a multi-rod geometry', under conditions 'where high circumferential strains are expected in order to reach mutual touching of rods'. However in the 3 x 4 array of rods only the two interior rods could be pressurised and the rod spacings were such that the deforming rods would need to strain about 65% before contacting the other rods. The tests were carried out by first heating to 350°C and pressurising with helium, then after about 20 minutes the rods were ramped at 10-20 k/s to about 800°C and held until 90 s had elapsed or the central rod(s) burst. The temperature of the guard rods was kept constant by controlling the power to the internal heaters.

The first series had convective air cooling and the main findings were:

From tests in which only one of the two central rods was pressurised and the temperature of the guard rods was either higher or lower it was concluded that: 'At a lower temperature level of the surrounding rods the central rod shows the same burst behaviour as in a single rod test, as was expected. Burst strain and location of burst rupture are not influenced by the surrounding rods (Fig. 74). At a higher temperature level of the surrounding rods the burst behaviour is influenced in such a way that the burst rupture opening is directed towards the hottest rod in the neighbourhood. The locally hottest spot of the cladding determines the rupture behaviour, Fig. 75'.

From tests in which both central rods were pressurised and the temperature distribution in the bundle was rather homogeneous it was concluded that:

'With different as well as with equal internal pressure of both central rods no mutual influence on burst behaviour history and location and size of the ballooning was observed. With different internal pressure the rods burst in temporal sequence, with equal internal pressure the rods burst nearly in the same moment.'

In the second series air was forced through the test section (see Fig. 76a) this caused a 'downstream' shift of axial temperature and strain maxima, comparable to observations in single rod tests in flowing steam (Fig. 76b). This effect was enhanced if air velocity and thus heat transfer coefficient was increased otherwise the results were similar to
those in the first series. The maximum strain achieved in the multirod tests was intermediate between that of the directly heated creep rupture tests and the fuel rod simulation tests. This can be seen from Fig. 77 which summarises the burst strain data from the whole of the KFW investigations and includes for comparison multi-rod data from the ORNL and REBEKA tests.

6.3.2.3 Single and multi-rod testing at the Japanese Atomic Energy Research Institute (JAERI)

The rod size used in the JAERI tests, which have 7 x 7 arrays, represents those used in a 15 x 15 type of PWR fuel assembly. Table 10 gives details of the materials and design parameters. (126) Figures 78 and 79 are schematic representations of the test rig and heating systems whilst Fig. 80 shows the location of the thermocouples.

The heat source for the cladding in the more recent tests, i.e. number 5 onwards, was six W-3% Re wires, running through holes in a stack of alumina pellets. The minimum distance between the outer edge of the pellet and the surface of the wires was 0.05 in. The W-3% Re wire has a higher positive temperature coefficient of resistivity than either the Kanthal used in the ORNL heaters or Inconel used in the KFK heaters. Because of this a hot spot tends to cause an increase in local heat generation i.e. at 1000° a 50°C local increase in the temperature of the wire results in a 5% increase in local heat generation. (127)

The tests are conducted in a steam atmosphere but the flow rate was so low (see Table 10) in most of the tests reported here, that flow effects can be discounted. A higher steam flow, 5.79 g/cm² min, has been used in later work not yet reported in detail; however this is still lower than that used in the ORNL test series. (127)

The system for destructive examination of the test assemblies is unique; they are disassembled first into vertical rows and photographed, then into individual rods, and measured, then all the rods are reassembled into a 7 x 7 array set in resin and finally sectioned horizontally see Fig. 81.

The first series of tests, (126) summarised in Table 10, with the W-3% Re wire heaters and a close fitting unheated shroud, examined the effect of burst temperature in the high alpha and alpha plus beta phase regions i.e. 740-920°C on deformation with ramp rates of about 7°C/s. A typical test history is given in Fig. 82. There was a slight temperature gradient across the assembly presumably as a result of the unheated shroud which reduced the ramp rate in the outer rows, see Fig. 83, by 1.6°C/s.

Figures 84-87 show vertical sections of the assemblies after testing, whilst Figs 88-91 show the positions of the bursts in relation to the axial temperature distribution and the axial extent of strains greater than 34% i.e. that required for touching of similarly strained rods. There is a clear trend of greater deformation in the interior rods; as found in the 8 x 8 ORNL test (see Section 6.3.2.4).

The axial distribution of ballooning in Tests 5 to 8 is shown in Fig. 92 and details are given in Table 11. There is a close correlation
between the data on single rod deformation and burst temperature in other work, and the deformation in these tests i.e. maximum ballooning occurred in the two tests, where rods burst in the high alpha region to low alpha plus beta phase region. Figure 93 shows the burst locations in Test 5.

In the next series of Tests 9–14 the unheated shroud was replaced by an 8 x 8 array of heated but unpressurised rods. The location of these 'subheaters' and thermocouples is shown in Fig. 94. The heating rate was about 7°C/s and the pressure 50 kg/cm² i.e. similar to Test 5 which had produced the greatest flow restriction. In Test 10 all rods were heated, in Test 11 the central rod was not heated whilst in Test 12, 4 rods were unheated except by transfer of heat from adjacent rods.

The presence of the surrounding subheaters reduced the temperature gradient across the 7 x 7 array as can be seen in Fig. 95 which shows that the difference in heating rates for a corner rod and a centre rod is 0.4°C/s, compared with a difference of 1.6°C/s with an unheated shroud, see Fig. 57. This resulted in greater and more coplanar axial extension of the deformation, see Fig. 96. In Test 11 the deformation was co-planar but not as axially extended as in 10 or 12, (see Fig. 97).

In Test 12 with a steam flow of 0.37 g/cm² min and in which there were 4 unheated rods there were bulges greater than 34% extending for 225–240 mm in rods adjacent to unheated rods, see Fig. 98 and 99.

Test 14 was a repeat of Test 12 although the steam flow was fractionally greater at 0.39 g/cm² min. However the incidence of long ballooning was greater in 14 (see Fig. 100) resulting in a greater increase in cross-sectional area (see Fig. 101) 'Long balloons' were considered by Kawasaki to have been produced by mechanical restraint from adjacent rods causing other parts of the restrained cladding to deform.(126)

Investigation of the detailed temperature histories identified differences between the temperatures of adjacent rods in the two tests which appeared to be correlated with the extent of deformation. This led to an investigation(128) into the deformation of single rods surrounded by eight unpressurised rods in which the temperature relative to the deforming rod was varied. These showed that axially extended deformation occurred in the single rod when its temperature was higher or equal to those of the surrounding rods, (see Fig. 102). The axial extent also increased with increasing temperature difference. These findings were related back to the data from Tests 12 and 14 and it was concluded that the deformation behaviour of cladding in a multi-rod array was influenced by the relative temperature between the deforming portion of the cladding and the facing areas in adjacent rods (see Fig. 103).

Test 13 had a very slow ramp rate (0.6–0.82°C/s), with a steam flow of 0.39 g/cm² min and rods burst in the range 765–800°C. There was extensive co-planar deformation and bulges had been forced to adopt square cross-sections with rounded corners by mechanical interaction with their neighbours; the maximum flow channel restriction was 87.6%.(131)

In the final part of the programme the primary objective was to investigate the upper limit of coolant channel restriction,(132) To this
end a shroud was re-introduced around the test bundle but inside the 8 x 8 array of guard heaters, see Fig. 104; the clearance was 9 mm in tests 15 and 16 and 3.5 mm thereafter.

In tests 15-20 all the rods in the 7 x 7 array were pressurised to 50 kg/cm² because earlier work had shown that this produced the maximum ballooning. But in tests 21-24, four control rod guide tubes were substituted for pressurised rods in positions shown in Fig. 104. Two heating rates were used, 1°C/s and 7°C/s. The former produced the highest coolant flow area restrictions of 89 to 91%, see Fig. 105, with the latter the maximum restrictions were 87 to 89%, see Fig. 106, and the remainder in the range 80-85%. A cross-section typical of the maximum blockage region of these bundles is shown in Fig. 107. The blockage in some of the individual sub-channels is greater than 95% as measured by image analysing techniques.(133)

The series produced two very interesting results viz:

(1) Ballooning behaviour of rods located next to control rods was not different from other rods, though the azimuthal temperature gradients were large in the rods.

(2) The maximum coolant flow area restriction in a bundle with control rods was almost the same as that without control rods, and the axial extent of the highly restricted portion in the former was larger than that of the latter in both the 1°C/s and 7°C/s heating rate tests. There was also little effect of heating rate on the degree and extent of blockage cf Figs 105 and 106.

6.3.2.4 The single and multi-rod burst test programme at Oak Ridge National Laboratory (ORNL) USA

The current series of tests at ORNL sponsored by the USNRC started in 1975 and has the following objectives: effects of rod to rod interaction on failure behaviour, magnitude and geometry of resulting blockage patterns, rupture temperature-pressure inter-relationships, flow resistance coefficients as a function of flow blockage and to provide an internally consistent data set that is amenable to statistical analysis.

The Zircaloy-4 tubing used was from the master lot purchased specifically for use in all the NRC cladding research programmes.(134) The dimensions (10.92 mm o.d. wall thickness 0.635 mm) are intermediate between current PWR and BWR designs. The electric heater rod used to simulate the fuel has a spiral wound Kanthal heating element and a uniform heat flux profile.(196) In the outer stainless steel sheath there are four grooves spaced at 90° in which lie thermocouples attached at their ends to the inner cladding surface, see Fig. 108. The test specimens have a heated length of 915 mm and an internal gas volume of 49-51.5 cm³, which is the minimum that is attainable with this particular design. Of this volume 13% is in the heated portion of the annulus between the heater and cladding and 10% in the unheated portion, 33% in the pressure transducer and piping, and 44% in the end regions (mainly the upper) of the test specimen. This volume is about the same as used in the REBEXA fuel simulators but over twice that used in the current American PWR fuel (~ 17 cm³).
The single rod test series PS (preliminary tests) were scoping and development tests for the SR series which followed. These were carried out by heating the rod and test chamber, in which there was a small downward flow of super-heated steam, to 350°C using external heating only, i.e., simulator power zero. The rod helium pressure was next adjusted to the desired value and power was then applied to the simulator to initiate the transient of 28°C/s. The pressure in a rod was not controlled and rose slowly as the gas temperature increased, reached a maximum, and then decreased at an increasing rate, as the internal volume increased because of cladding strain, until rupture occurred. The rate of increase in temperature of the cladding near the rupture levelled off and in some cases diminished just prior to rupture, indicating strain cooling. The variation of circumferential strain with burst temperature in the above tests is given in Fig. 109. Also there are results from tests in argon which are similar at temperatures < 950°C (i.e., where oxidation effects at this heating rate are minimal) but show greater strains above this level. In two tests the gas volume of the specimens was increased to 160 cm³, i.e., 4 times the normal value without apparently any effect. This may have been an attempt to justify the use of 2½ times larger volume in the standard ORNL test specimens compared to that used in American PWR fuel rods.

Figure 110 gives the axial length changes of the specimens as a result of testing. The effect of strain anisotropy causing the tubes to decrease in length in the alpha phase region is clearly demonstrated. In addition to the ramp tests at 28°C/s four creep rupture tests were carried out in steam at about 760°C with internal gas volumes of about 51 cm³. The times to rupture at 760°C varied from 49 to 250 s and the strains from 23 to 32%. Two of these fuel simulators were then used for transient tests at 28°C/s which burst at 760-770°C, with strains of 20 and 23%. Ramp tests were also done at 5 and 10 K/s. The strains in all these tests were not significantly different from those ramped at 28°C/s, see Fig. 111, but there was a tendency for greater volume increase with decreasing ramp rate, see Fig. 112. The first multi-rod test (197) (B-1) was carried out in 1977 using a 4 x 4 array of heated and pressurised rods in the test assembly shown in Fig. 113. The two centre grids are 56 cm apart, a spacing which is similar to that in reactor fuel assemblies. The 4 x 4 array of rods was surrounded by a direct resistance heated shroud. To avoid electrical shorting between the test rods and the shroud there was a gap of 16 mm in between them. There was thus no restraint on the outer row of rods. The rods in B-1 were ramped, at 29.5°C/s and the shroud at 20°C/s, in flowing steam (about 4.2 kg/h, Reynolds number 250) from 350°C until the rods ruptured at about 850-880°C, all within about a time span of about half a second. The circumferential strains in the burst region were 32-59% which is significantly greater than achieved in the single rod tests at nominally the same conditions, see Fig. 114. The rods had moved outwards from their centre-line position, reflecting the lack of restraint. The flow blockage was 49%, see Table 12.

The second multi-rod test B-2, (198) ramped at 28.5°C/s, steam flow Reynolds number 290, was done with nominally the same conditions as B-1 except that the shroud was not heated. The results were similar; burst strains 34-58% (138) see Fig. 114, flow blockage 54% (see Table 12) except that there was greater bowing of the rods which was attributed to the
larger circumferential temperature gradients resulting from the presence of an unheated shroud.

The primary objective of the next multi-rod test, B-3(139) was to 'investigate bundle deformation under test conditions believed by some to be more realistic of PWR LOCA parameters' i.e. the temperature ramp rate was reduced to about 9.5°C/s with the initial pressures adjusted to cause rupture at about 760°C. This test was done with a heated shroud (ramp rate 7.1°C/s) and the internal volumes ranged from 49-51.5 cm³. The steam flow was the same as used in B-1 i.e. about 4.5 kg/h and the Reynolds number was 263. The rods in B-3 failed at about 764°C wholly in the alpha-phase region. The maximum strain was 77% and six rods had strains greater than the maximum of either B-1 (59%) or B-2 (52%), see Fig. 114 (and much greater than single rods heated at 10°C/s (cf Fig. 115)). There was greater overall deformation with many portions of the assembly straining greater than 32%, see Fig. 116. The maximum flow blockage was about 77%, see Table 12. The ruptures all occurred within the main intergrid region, unlike B-1 and B-2 where bursts occurred in both intergrid regions. The same fuel simulators were used in B-1 and B-3 in exactly the same orientation and position so a direct comparison can be made between these two tests, see Fig. 117. There are known hot spots in the internal heaters and these can be correlated with burst positions, presumably in B-3. The heat transfer-strain/rate combination was such as to suppress the effects of hot spots in the upper part of the fuel simulators.

The effect of steam flow was investigated further in two single rod tests(138) with grids attached, using the heater fuel rod number 4 in bundle test B-2. The first test had the steam flow normally used in single-rod tests i.e. Reynolds number 800 and in the second, the steam flow (Reynolds number 180) was lower than that used in the B-2 bundle test (Reynolds number 290). The results (see Fig. 118) showed that at the low steam flow (Reynolds 180) the fuel simulator caused the Zircaloy cladding to burst in the same position as did the cladding in the multi-rod test B-2 (Reynolds 290) whereas with the higher steam flow the position of burst was shifted along the rod in the direction of steam flow. Further evidence for this effect is shown in Fig. 119.

The maximum strain produced by the same fuel simulator in the multi-rod test B-2 was about 42% whereas in the single rod test it was about 28%. This indicated that radiation from surrounding rods may be influencing the strain so a series of single rod tests using heated shrouds was carried out. These showed that at ramp rates of 28°C/s the strains were not much greater than in tests with unheated shrouds, see Fig. 120. However, heated shroud tests carried out at KFK did show greater strains at high ramp rates, 24-30°C/s,(141) which were equivalent to those produced in the ORNL multi-rod tests B-1 and B-2 ramped at about 29°C/s, see Fig. 120.

The strains in single rods ramped at 10 K/s, at ORNL and KFK with heated shrouds were similar to those achieved in the B-3 multi-rod test with a 9.5 K/s ramp, see Fig. 115.

The target conditions for the next multi-rod test B-4(142) with a 6 x 6 array were ramping at 5 K/s, i.e. to about 800°C, but malfunction of the equipment with bursting in the peak ductility region for alpha phase
prevented the rods from achieving this and the maximum strain reached before the test was terminated was only about 20%.

Test B-5(199) was conducted under conditions close to B-3 (4 x 4) 10 K/s, see Table 12 but with an 8 x 8 array of heated and pressurised rods surrounded by an unheated shroud spaced at one half of the coolant channel distance from the outer rod surfaces, i.e. much closer than in previous tests, to obtain information on the effect of test array size on deformation and rod-to-rod interactions. The shroud was constructed from stainless steel (0.1 mm thick) backed by insulating material and a strong structure to withstand any radial forces from expansion of the fuel rod simulators. The shroud could not be directly electrically heated because of the close proximity of the fuel rod simulators so the inner surface was gold plated and polished to reflect radiant heat back to the test array; even so its temperature increased significantly. The rods in the 8 x 8 array were pressurised after holding at 336°C to achieve thermal equilibrium, but one rod in the outer ring failed to hold pressure and the test was conducted with this rod heated but unpressurised. All rods except this one burst within six seconds at about 775°C.

The superheated (355°C) steam flowed downwards at about the same mass flow as in B-3, about 288 g (s m⁻²)⁻¹, a Reynolds Number of 140 as it entered the test array through a single side nozzle. Unfortunately, under those conditions a uniform steam distribution could not be established, unlike previous tests with 4 x 4 and 6 x 6 arrays, resulting in a gradient across the bundle of 24°C at the top reducing to 2°C at the bottom. (Note the steam distribution system has since been modified to avoid this effect.)

The non-uniformity of the steam flow had an effect on the cladding temperature during the test in that there was a 20°C gradient at the 5 cm level, also the rods on the north side had an initial average axial temperature gradient of 11°C whilst those on the south side had a gradient of 2-3°C. Figure 121 shows that the average temperature of the outer ring of fuel simulators was lower than those in the interior and also demonstrates the north-to-south and east-to-west gradients. Figure 122 shows the temperature differences between interior groups of rods. Visual examination after testing showed that cladding deformation was greater on the north face than the south face. However, the gaps between all the outer rods in both faces were completely closed in places for axial distances of 5-10 cm. None of the bursts on the exterior rods were directed towards the cooler shroud and most of the interior bursts were towards the coolant channel, see Fig. 123. Examination of the cross-section(143) from the bundle showed evidence of rod-to-rod interaction producing trapping of the bulges causing them to become square, see Fig. 124. Some of the strains of the individual rods are given on Fig. 124, the others can be estimated by comparing their dimensions with the outer rod which did not strain, and the scale. Thus the cladding strains in the rods which formed 'square' cross-section are in the range of about 50-75%, whilst others which are still reasonably circular but are trapped between two or more rods have strains in the range of about 25-40%. The trapping of bulging rods appears to have caused the deformation to extend axially resulting in a greater volume increase, see Fig. 125 and greater flow restriction, see Figs 126 and 127, than had been seen in the B-3 test with similar maximum strains see Figs 124 and 128. Thus the
authors conclude that flow area restriction in large arrays may be underestimated by small unconstrained arrays and that two rows of deforming 'guard' specimens are necessary around the deforming array under investigation to simulate the radial temperature and mechanical boundary conditions which would be present in a large array of rods such as a fuel assembly. The extent to which this applies in conditions of high heat transfer has been argued to be small, however.

In the concluding B-6(144) test in the ORNL programme, carried out in December 1981, an 8 x 8 array with a closely fitting, unheated, shroud was ramped at 3 to 4 K/sec to fail in the region of 930°C, i.e. well into the alpha plus beta phase region, to ascertain if the typically smaller strains seen in single rods burst under these conditions persist in large arrays. The results(144) showed this to be so, the burst strains ranging from 22 to 56% with an average of 30% consistent with an average of 36% for three single rods tested under similar conditions. The coolant channel flow area reduction was modest: 39% for the whole 8 x 8 array, 44% for the inner 6 x 6 and 46% for the central 4 x 4 array.

6.3.2.5 Single and multi-rod testing at Westinghouse Electric Corporation USA

These tests(145) were carried out using standard Westinghouse fuel rod cladding, 15 x 15 size Zircaloy 4 tubing for the multi-rod tests and both 15 x 15 and 17 x 17 sizes for the single rod tests.

The single rod tests were carried out in air and flowing steam using specimens filled with alumina pellets and heated externally in a radiant heater furnace which produced a uniform hot zone at various heating rates in the range 5 to 200°F/s.

The results, see Fig. 100, show that the burst strains of the specimens heated at 5 and 25°F/s, with internal pressures typical of those 'at power' in current PWR fuel rod design, i.e. 900-1200 lb/in² are in the range 40-110%. These heating rates are the closest in this test series to the rate of about 15°F/s which is calculated to occur during adiabatic heat-up following a large break LOCA.

The 4 x 4 multi-rod tests used direct resistance heating of the cladding which had been coated with ZrO₂ in an attempt to to prevent electrical arcing between rods. Unfortunately, post-test examinations revealed localised cladding melting, implying that the ZrO₂ coatings had cracked during swelling. Only two internal pressures were used, 200 and 2250 lb/in² of which the former would cause the cladding to burst in the alpha plus beta phase region and the latter would produce very high strain rates. Neither of these conditions is conducive to developing the maximum strain potential of the Zircaloy cladding as can be clearly seen from the single rod results, see Fig. 129. The maximum blockages resulting from the multi-rod (4 x 4) tests are about 50%, see Fig. 130.

6.3.2.6 Single rod testing at Argonne National Laboratory (ANL) USA

An extensive investigation has been carried out at ANL(146) on the deformation characteristics of Zircaloy-4 PWR cladding using various heating rates, pressures, atmospheres and axial mechanical constraint.
Specimens tested in a vacuum exhibited strain peaks at three temperatures; however the two high temperature peaks were suppressed as a result of oxidation when the tests were repeated in steam, see Fig. 131.

There is a considerable effect of axial restraint during tube burst testing on the maximum circumferential strain in the high alpha phase region, see Figs 131 and 132. This results from strain anisotropy which operates freely in unconstrained tubing allowing material to flow axially into the bulging region as manifest by reductions in specimen length after testing. The cladding tubes containing mandrels can shorten by 2.5 mm because the mandrels do not completely fill the specimens and show strains intermediate between those of the empty tubes and the pellet-filled ones which cannot shorten.

The rupture strains are greater in specimens tested in steam compared with those tested in vacuum for heating rates < 50°C/s, see Fig. 131; similar results have been observed in other work (107) and are considered to be a result of surface oxidation although the exact mechanism has not been precisely identified. In the alpha-phase, the rupture strains are greater.

The data from this programme have been combined to produce a three dimensional plot, see Fig. 133, which shows that in steam circumferential strains > 0.4 will result for internal pressures > 6 MPa and heating rates < 25°C/s.

6.3.2.7 Single rod testing at Saclay - CEA - France

An extensive programme (EDGAR) has been carried out on internally pressurised Zircaloy tube specimens (500 mm long) using direct heating, (201) to investigate the behaviour of fresh and irradiated materials (stress relieved and recrystallised Zircaloy) under postulated LOCA conditions.

The first objective of this programme has been to verify the actual behaviour of Zircaloy cladding under conservative calculated large break LOCA two-peak transient conditions, such as those considered in the French 900 MW(e) FWR standard safety reports. The interpretation of the Zircaloy behaviour has shown that the recrystallised material is slightly more creep resistant than the stress relieved material.

The second objective of the EDGAR programme has been to supply experimental data for the development of a deformation model and a burst criterion, which constitute a part of the data basis of the CUPIDON code (169). The experimental data used for this modelling are retrieved from more than two hundred individual tests either with constant pressure and constant heating rate (0.2 to 100°C/s) or with constant temperature and constant pressure rate (0.01 to 0.2 MPa/sec) or creep tests. Some specific tests have been added to take into account the effect of a thermal spike into the α-β or β phase on the subsequent mechanical behaviour of the cladding during the two-peak transient.

The third objective is to study the influence of irradiation on the mechanical behaviour of Zircaloy. For that, an EDGAR rig has been set in a hot cell in SACLAY. This rig has exactly the same design as the one
used for tests with fresh material and permits a direct comparison of the behaviour of fresh and irradiated material under similar conditions. In particular, the deformation of irradiated cladding is continuously measured during the test. The first transient tests performed with Zircaloy cladding retrieved from spent fuel rods have shown a larger creep rate of the irradiated material for the first ten seconds of the two-peak transients. Moreover, the ultimate hoop stress of the irradiated cladding decreases at burst in the 800–950°C range from 15–20% compared with fresh material. At present, several different types of test are planned to understand the mechanism of annealing of irradiation-induced defects under fast temperature and differential pressure transient conditions and ultimately to take into account this effect in the modelling of the Zircaloy behaviour under LOCA conditions.

6.3.2.8 Single and multiple rod testing in the UK

A test series has been carried out at the Springfields Nuclear Laboratories on short internally pressurised Zircaloy tube specimens heated by direct resistance heating to investigate deformation behaviour in steam and inert atmospheres. This work showed the dramatic effect of oxygen pick-up on mechanical behaviour above about 1150K with oxide and stabilised alpha-phase layers contributing mainly to the increased strength and resulting in reduced circumferential strain owing to localised straining at cracks in these layers. Very small amounts of oxidation were also found to influence the character and extent of deformation at about 1020K. Following on from this work, tests on larger specimens showed the phenomenon of axially extended deformation under both mainly radiative cooling conditions and mainly convective cooling see Fig. 134. This has been investigated in depth and supported by similar studies at Berkeley Nuclear Laboratories. The basic process responsible is that of local cooling of the straining region combined with the high temperature – dependency of secondary creep in Zircaloy; however other factors can come into play as shown in Fig. 135.

Tests have been carried out using internal heaters with a reflective but unheated shroud and similar results have been obtained except that the circumferential strains tend to be lower. However when heated shrouds are used the strains are equivalent.

The full range of internal pressures which could be used in PWR fuel rods has been investigated and, depending upon the temperatures reached, extended deformation occurs over the whole pressure range. A tendency for super-plastic deformation in the two-phase region has also been observed with pressures of about 300 lb/in². Cladding strained in this region, under fast strain rates greater than 5–10 k/s, exhibits a ductility trough; however this is a strain-rate-dependent phenomenon, see Fig. 136, and strains of 200% have been produced in this region in a test lasting 200 s.

Stainless steel cladding as used in PWRs i.e. Type 304 has also been investigated. This material is strong enough to resist the internal pressure at temperatures up to about 950°C so that deformation is negligible. Ultimately at higher temperatures it will deform but the inherent ductility is such that the maximum strains are 25–30%, insufficient to allow mutual support of columnar bulges, so that coolant
flow channel restriction would be very limited.

At the Windscale Nuclear Laboratories 450 mm lengths cut from commercial PWR fuel rods irradiated to 20 GWD/tU (rod average) have been internally pressurised and subjected to temperature transients to simulate LOCA conditions.\(^{(154)}\) The primary purpose of the work is to examine the mechanical stability of the fuel column when the cladding has bulged away from it under internal pressurisation and to evaluate the extent of fuel fragmentation and re-location that occurs. However data on the ductility of cladding which has experienced actual reactor service has also been produced.

In the fuel re-location tests, carried out at 973 to 1073K, the cladding deformation was stopped upon reaching approximately 40% diametral strain by a reduction in temperature and, after cooling, the fuel was fixed in position by introduction of epoxy resin through a spark-machined hole in the cladding.\(^{(154)}\) The spatial distribution of fuel was then determined by X-radiography, gamma scanning and optical examination of sections.

The lower 100 mm approximately of the cracked fuel stack in each test-piece was shown to have remained in the form of pellets, although the fuel column had moved across to touch the cladding. Above this region the pellet fragments had separated, often quite considerably, but in general only a relatively small amount (less than 5%) of fuel axial relocation had taken place; for instance, the positions of pellet-pellet interfaces were still visible on axial sections. A typical radiograph print is shown in Fig. 137.

The deformation of the cladding in these tests was closely comparable with the axially extended bulging behaviour observed during direct electrical heating tests carried out on unirradiated Zircaloy tubing at the Springfields Laboratories\(^{(136,152)}\) confirming that no significant difference in strain behaviour exists between unirradiated and irradiated cladding, even though the latter presumably has an additional thin oxide layer resulting from reactor exposure.

The multi-rod test programme at the Springfields Nuclear Laboratories has included a materials test in which a 4 x 4 array contained in a shroud was heated to about 700°C in argon in a muffle furnace. Steam was then introduced and the rods pressurised to 7.9 MPa. Examination showed that they had bulged and tended to adopt a square cross-section as a result of rod-to-rod interaction, see Fig. 138. The object of this test was to study mechanical interaction of balloons to assist code development.\(^{(163)}\)

The use of high and low helium filling pressures in alternate fuel rods in an assembly has been proposed as a means of reducing the probability of a high degree of co-planar blockage.\(^{(155)}\) Computer modelling\(^{(156)}\) of the deformation of such an assembly has shown that for this concept not to be effective, 80% overall strain would be required in the straining rods. This requires the individual cladding temperatures all to be uniform to within 4°C. The evidence from the MT-3 test\(^{(108)}\) and
analysis (157) of irradiated PWR fuel rod ballooning tests indicate that such temperature uniformity is unattainable in nuclear heated rods ballooned under reflood conditions.

6.3.2.9 Measurement of blockage

The generally accepted definition of blockage (or coolant channel restriction) is given below and shown schematically in Fig. 139.

$$BZ = 100 \times \frac{\text{difference in cross-section between swollen and unwollen rod}}{\text{original coolant channel cross-sectional area}}$$

This definition gives B = 0% for no deformation and B = 100% if all the rods deform into squares of side p, where p is the rod pitch, see Fig. 140a and Fig. 140b.

The extreme case of 100% general blockage is not obtainable experimentally. However, at reasonably high rod strains the situation depicted in Figure 140c can be obtained, i.e. where swollen rods extend beyond the boundaries defined by the rod pitch. This situation is observed when the rods are not restrained by neighbours or a surrounding shroud and the strain is greater than 33%, or where the rod has moved from its original position in the bundle.

In this situation two options can be exercised in the evaluation of final rod area:

1. Total area method

   Measure the area of each rod in the deformed bundle. Then use the blockage definition shown in Fig. 139.

2. Mask method

   Assume that the blockage should be evaluated only within the area defined by the original coolant channel area, see Fig. 141. Then use the blockage definition shown in Fig. 139.

The first method, 'total area method', makes no allowances for movement of the rods. Thus an array of swollen rods, each of which has moved, leaving an 'open lattice', has exactly the same numerical blockage value as an array in which the individual rods have deformed to the same extent but have not moved, when obviously there is a difference in coolant flow area, see Fig. 142a. In addition, 'local' blockage values of > 100% are perfectly feasible, see Fig. 142b, if the rods swell beyond their lattice spacings. Also if the outer rods of a highly deformed bundle swell beyond the original lattice dimensions, total blockages > 100% can be obtained e.g. as reported (158) for the MT-4 bundle. In both these situations the results although mathematically correct, can therefore be misleading since 100% blockage cannot be coolable and a blockage of > 100% is not physically meaningful.
The second method, the 'mask method' cannot produce total blockage values greater than 100%. However unless the deformation in each rod at the same plane is uniform, which in practice it is not, it does not provide an adequate description of the change in individual coolant channels e.g. the general blockage 'mask' value for the cross-section shown in Fig. 107 is about 85%, see Fig. 106, whereas the individual channels are reduced by 96–98%.(150) Similarly the PHEBUS array (Fig. 46) has an overall blockage ratio of 48% but the area of one sub-channel has been reduced to 80% of the original.(150) What is required for heat transfer calculations is a measure of how much of the original coolant channel area remains between four rods when they have distended to form either a closed or open sub-channel, because it is this which has a major effect on the temperature of the cladding surfaces in that sub-channel. It is also important to know the perimeter of sub-channels and the extent to which they are closed.

A mathematical description of sub-channel blockage is given in Fig. 143, sub-channel perimeters and rod gaps are dealt with in Fig. 144. Proposals for dealing with the various types of sub-channels and burst openings are given in Figs 145 and 146.
7. PREDICTIVE COMPUTER CODES

7.1 INTRODUCTION

Workers in several countries have developed codes aimed at predicting the behaviour of a fuel assembly in a LOCA. In general the code attempts to predict the deformation of a fuel rod, the termination of deformation by rupture, the temperature reached by the cladding, and in some codes the interaction between neighbouring rods. A code typically draws input information on coolant condition from a thermalhydraulic code, and data on fuel from a steady-state code, while a range of sub-codes calculate fission gas release, deformation etc. Leading examples are considered below.

7.2 FRAP-T

One of the best-known and most widely-used systems is the series of codes FRAP-T (Fuel Rod Analysis Program - Transient) developed by EG & G, Idaho, USA. The version FRAP-T5(159) calculates the transient response of a single fuel rod under a range of accidents ranging from operational transients to LOCA's and reactivity insertion accidents. It is a modular code comprising several sub-codes which interactively calculate the effects of fuel, cladding and plenum temperatures, fuel and cladding deformation and rod internal pressure, including fission gas release. The interaction scheme is shown in Fig. 147; the rod response is determined by cycling through an outer and inner loop. In the outer loop fuel rod temperature and deformation are alternately calculated. On the first cycle the gap conductance is calculated from the fuel/ clad gap. The distribution of temperature in the fuel rod is computed, and hence deformation. A new gas gap is computed, and the cycle is iterated to convergence. In the inner loop the internal gas pressure is the variable found by iteration; the temperature distribution remains constant while deformation, gas gap and plenum volume are computed. Pressure is calculated and passed to the deformation calculations. After these two loops fission gas release is determined.

Input data is taken from a steady-state code such as FRAP-S or FRAPCON. The coolant conditions are obtained from a code such as RELAP.

Deformation calculation uses a sub-code FRACAS, with a separate ballooning sub-code. A further sub-code FRAIL (FRAP integrity limit) predicts failure of the cladding and calculates blockage of the sub-channel between rods. Fission gas release is derived from the sub-code GRASS.

The latest version FRAP-T6(160) incorporates various refinements; an improved sub-code BALON-2 and a better fission gas release model FAST GRASS. The complex inter-relation of sub-codes just described may be considered typical of fuel deformation codes.

7.3 SSYST

This code(161) has been developed in the German Federal Republic at the Institut fur Kernenergetik und Energiesysteme, Stuttgart and the Kernforschungszentrum Karlsruhe. Three versions have been produced
successively; SSYST-3 draws input from the steady-state code COMETHE(162) and the thermal-hydraulic code RELAP4/MOD6. The refill and reflood conditions are derived from modules WAK and REFLOWS, and sub-modules calculate heat transport, gap conductance, gas pressure, rod deformation (STADEP) and Zircaloy oxidation (ZIRKOX).

SSYST has been used to model reactor loop experiments such as FR-2(87) and out-of-reactor rigs such as COSIMA and REBEKA.(90 et passim) The code is used to predict core behaviour in a LOCA, including a probabilistic analysis.

7.4 MABEL

MABEL(163) is an interactive thermal-hydraulic - deformation code developed in Great Britain by the UKAEA. It is composed of modules, i.e. groups of subroutines which are closely interlinked, that calculate the interrelated effects of subchannel heat transfer, fuel and cladding temperatures, rod internal pressure and cladding deformation.

The calculation procedure is illustrated in Fig. 148 which shows a simplified flow chart of MABEL-2. The calculations begin with the code processing the input information, much of which may have been obtained directly from a fuel performance code such as SLEUTH-78,(164) MINIPAT(165) or HOTROD(166) and a thermal-hydraulics code such as RELAP4-MOD6(167) or TRAC.(168) Next, a steady-state calculation is performed to determine the fuel rod conditions at the start of the transient. Then the time is advanced and a transient solution is performed. The length of the timestep can either be set via the Input Data or can be calculated within the code so that long timesteps are used when the rates of change of cladding strain and temperature are low and short timesteps are used when the rates of change are high. Transient timesteps then continue until the specified end of the transient is reached.

Thermal-hydraulic data for MABEL-2 would normally be obtained from codes such as RELAP-4-MOD6(167) and TRAC.(168) The information provided from these codes defines data such as heat transfer coefficients, mass velocities and heat-to-coolant powers as functions of time for a reactor average channel in a loss-of-coolant accident. These data form the basis for the sub-channel heat transfer coefficient calculations in MABEL-2.

In the transient calculations there are strong inter-relations between a number of variables, which are most important of which are sub-channel heat transfer coefficients, fuel and cladding temperatures, fuel rod internal pressure and cladding strain. For example, the deformation of the fuel rod cladding affects the rod internal gas pressure since the internal volume available to the gas is changed. It also affects the temperature in the fuel and cladding because the flow of heat from the fuel to the cladding is dependent on the size of the fuel-to-cladding gap and the sub-channel geometry has been altered. These and all other interactions are taken into account.

7.5 CUPIDON

The code CUPIDON(169) has been developed at the Centre d'Etudes Nucleaire de Saclay, France. Input data on the fuel is taken from the
RESTA or DEMETER codes, and on thermalhydraulics from RELAP. Flux depression, gap conductance, internal pressure and cladding oxidation are modelled to calculate deformation. The code is being validated against single rod tests in the EDGAR rig,(201) out-of-reactor, and the FLASH reactor loop,(202) and against the PHEBUS(102) in-reactor multi-rod experiments.

7.6 CARATE

This code has been developed by KWU for the conversion of experimental results into a deterministic clad creep and burst model which can then be used in codes for LOCA analyses. Thermalhydraulic boundary conditions must be input. The following quantities are considered in CARATE to be sufficient for their definition: (1) differential pressure as a function of time, (2) cladding temperature as a function of axial and azimuthal position and of time, and (3) wall thickness at start of the transient as a function of axial and azimuthal position.

The code includes an appropriate creep rate law for the Zircaloy tube material and an algorithm to treat local variations in temperatures, wall thicknesses, stresses and strains as well as a suitable, locally applicable burst criterion. A detailed description of the code has been presented by Eberle et al.(170)

7.7 ACCREL-2

This code has been developed by the Technical Research Centre of Finland,(171) It is a fast running, modular computer code suitable for core-wide fission product release studies. It first simulates deterministically the status of the reactor core during normal operation and then continues to calculate probabilistically by Monte Carlo techniques the number of fuel rods rupturing and the amount of radioactive fission products released from the ruptured rods during a reactor transient.

7.8 VALIDATION OF CODES

The prediction of the behaviour of fuel rods, or fuel rod simulators, which have been used in experiments simulating LOCA's is a difficult task given the complexity of the system being modelled. The starting-point - the discharge in two phase flow of the coolant from the reactor circuit - is itself complex enough. The succeeding phenomena of refill and reflood of a core which is a mechanically complex structure are also inherently difficult to model. The deformation of fuel rod cladding is not difficult to predict under well-defined conditions, but the temperatures reached by the fuel rods are subject to considerable uncertainties. These derive from two sources - the lack of symmetry and regularity in the fuel stack which is the source of heat, and the complex mode of heat removal from the cladding by a coolant with a liquid and a vapour phase not in thermal equilibrium.

Progress is being made in the modelling of these phenomena. In addition to the references already quoted, the reader is referred to the proceedings of the IAEA Specialist Meetings on Fuel Element Performance
Computer Modelling (172-175). The CSNI has organised International Standard Problem 14, (113) based on a test in the REBEKA rig at KFK in 1983, submissions for which are currently being evaluated.

8. COOLABILITY OF DEFORMED ASSEMBLIES

The experimental Data from many laboratories which have been reviewed in the preceding sections show that cladding can deform, embrittled and fracture when subjected to severe temperature transients. In assessing the probability of an assembly’s remaining coolable under the action of the emergency core cooling systems, it is usual to combine data from separate deformation and thermal-hydraulic experimental series, since it is difficult to instrument experimental assemblies in sufficient detail while ensuring that, for example, thermocouples measuring cladding temperatures do not themselves inhibit deformation. Work is still proceeding along these lines, as exemplified by refs 4-6. The assessment of such programmes is outside the scope of this paper, and is itself a subject of considerable complexity. We believe that workers in the field are of the opinion that the magnitude of deformation which is seen to occur in multi-rod assemblies such as those described in Section 6 does not preclude cooling by the reflooding emergency core cooling water (see Section 9 below).

9. CREEP OF ZIRCALOY-4 PWR FUEL CLADDING TUBE

The creep of zirconium and its alloys of which Zircaloy-4, the preferred material for PWR cladding, is one, has been extensively studied for the past twenty years. Over this time it has become increasingly apparent that creep in these materials can be affected by a number of variables.

A recent study (176) of creep in 'pure' zirconium, i.e. iodide crystal bar (oxygen 115-150 ppm, tin 10-40 ppm, iron 110-370 ppm, chromium 40-50 ppm, nickel 30-35 ppm) has shown that at 600°C the stress exponent n increases from about 3 to 9 as shown by the non-linear log ε (strain rate) vs log σ (stress) plot shown in Fig. 149. Also the activation energy for creep decreases from about 3.8 eV to 2.8 eV as the applied stress increases from 5 to 20 MPa, see Fig. 150. The creep rate is also texture-dependent, see Fig. 151 increasing as the number of basal poles in the tensile direction is decreased.

Zircaloy-4 is an alloy of zirconium containing tin which was added to overcome the deleterious effects on corrosion, in aqueous environments, of the impurities, nitrogen in particular, present in the less expensive 'sponge' material made by the Kroll process, (177) instead of the vastly more expensive 'pure' crystal bar metal made by the Van Arkel iodide process. (177) A level of 1.5 W/o tin was sufficient to neutralise the effects of 1000 ppm nitrogen, which is considerably more than is likely to be found in sponge material, and the specification was set at 1.2 to 1.7 W/o. An additional advantage of tin is that it increases the high temperature strength properties of zirconium. (178, 179) The other alloying elements are: iron 0.18 to 0.24 W/o, chromium 0.07 to 0.13 W/o and nickel 0.007 W/o. The latter value is close to the maximum impurity level. The first two impart corrosion resistance and increase the recrystallisation temperature, (180) but their levels are constrained by their effects on
fabricability.

Oxygen, which is present as an impurity in solution, is limited to 1600 ppm to avoid problems in fabrication, increases the high temperature strength (181-184) but little is known quantitatively at levels appropriate to the specification i.e. 900-1600 ppm.

In addition to the metallurgical effects of tin and oxygen there are variations within the manufacturing tolerances which promote scatter in properties. (185) Hunt (186) has assessed these in terms of the ratio of predicted strain to standard strain, see Fig. 152.

Variations in strength due to the effects of cold work and/or irradiation can fortunately be ignored for the purposes of calculating mechanical behaviour during a LOCA because recrystallisation occurs within a few seconds of reaching 700°C (94) and thus strain calculations can be based directly on out-of-reactor experimental data. However, in view of the effects on creep of texture and chemical composition the material used for such tests must represent that currently in production for nuclear fuel cladding. The stress level must also be appropriate.

An added complication is that at about 820°C alpha phase Zircaloy-4 begins to transform to beta phase. This progressively changes the strength of the metal until the transformation is complete at about 1000°C and in this region superplastic behaviour can occur if the strain rates are low. Partial superplastic behaviour has been observed at strain rates relevant to those appropriate to some LOCA situations for example. (152) Rosinger et al (187) for example have done experimental work and surveyed secondary creep data of Zircaloy-4 cladding tubes to provide equations for creep in all three phase regions.

Stress rupture tests (188) on Zircaloy-4 PWR tubes of the 17 x 17 type have shown that their rupture life \( t_R \) can be predicted approximately using the simple relationship \( \varepsilon_i \cdot t_R = \varepsilon_i^{n} \) where \( \varepsilon_i \) is the initial secondary creep rate and \( n \) the stress exponent for creep, if their deformation follows the standard Norton type power-law relationship.

A detailed study (189) of the alpha-phase region creep behaviour of Zircaloy-4 cladding tubing has shown that the stress exponent \( n \) is temperature dependent decreasing from \( \sim 5.9 \) at 973K to 5.2 at 1073K. Also the apparent activation energy decreases from 372 KJ/mole at an applied stress of 10 MN/m\(^2\) to 280 KJ/mole at 60 MN/m\(^2\) in the range 973-1073K. Further work (190, 191) has shown that the behaviour of tubes from one manufacturer and within one batch can be very consistent but that unexplained but systematic differences can arise between tubing from different manufacturers. Hence, if very accurate modelling of deformation behaviour is required the creep equations used must directly reflect the properties of the material being modelled. Models which aim to predict the behaviour of cladding over a range of temperatures, say, 700°C to 1200°C, must take into account not only the occurrence of the transformation to beta phase but the rate at which this occurs, because rapid temperature increases can shift the effective temperature of transformation. They must also account for strain rate effects in the two-phase region where there is a tendency to superplastic deformation particularly in the range 850-870°C. Above about 850°C the effects of oxidation must be modelled because the
presence of oxygen increases the transformation temperature of alpha to beta phase so that a growing rim of alpha phase layer is superimposed on the alpha + beta or beta phase material which constitutes the remainder of the tube wall. The presence of an oxide layer imposes a stress on the tube (192) which can cause diametral increases of up to one per cent at 840°C in 500 s (193). Ultimately the oxidation affects the ductility and lowers the total circumferential rupture strain to a few per cent (147).

10. DISCUSSION, EVALUATION AND CONCLUSIONS

In the previous sections the factors governing the deformation of FWR fuel have been described, and the principal experimental programmes studying the topic have been summarised. The data accumulated in these programmes may be summarised as follows:

In the range of stresses and temperatures which may be produced in accidents, strains in the range 30%-90% can be produced.

The behaviour of cladding in a temperature transient is strongly influenced by the temperature distribution spatially. This distribution is in turn dependent on heat transfer mechanisms at the surfaces of the cladding. Meaningful experimental simulation must therefore accurately reproduce these mechanisms. This implies the use of realistically heated fuel rod simulators, realistic conditions of surface heat transfer, and the use of multi-rod assemblies to reproduce the heat-transfer conditions in the sub-channels between rods.

We conclude that:

Co-planar deformation with strains up to and including those leading to mechanical interaction between fuel rods have been demonstrated experimentally. No experiment realistically simulating a design basis LOCA i.e. one with a multi-rod array and simulated reflood cooling, has produced deformations which would inhibit quenching.

Such experiments have not as yet covered the entire envelope of conditions which might obtain following a LOCA. Nor has it yet been determined experimentally what degree of deformation and sub-channel closure would be needed to cause coolability to be lost. Experimental programmes now under way should resolve these questions.

Data from the experimental programmes should continue to be applied to the validation of predictive codes. Most of the experimental work summarised in this report has been concerned with the consequences of a large-break LOCA. More data is being and should continue to be obtained on the behaviour of fuel following a small break.

11. ACKNOWLEDGEMENTS

This review has drawn on data and figures from many laboratories, and the authors wish to express their thanks to the following organisations for the supply of such data and for helpful comments on the first draft:

AUSTRIA
Österreichisches Forschungszentrum Seibersdorf
CANADA
AECL:
CANDU Operations, Sheridan Park
Chalk River Nuclear Laboratories

EEC
Joint Research Centre, Ispra

FEDERAL REPUBLIC OF GERMANY
Kernforschungszentrum Karlsruhe
Kraftwerk Union, Kernbrennstoff Kreislauf
Technische Universität München

FINLAND
Technical Research Centre of Finland

FRANCE
Commissariat à l'Énergie Atomique:
Centre d'Etudes Nucléaires de Grenoble
Centre d'Etudes Nucléaires de Saclay
Institut de Protection et de Sureté Nucléaire

JAPAN
Japanese Atomic Energy Research Institute

SWEDEN
Studsvik Energiteknik AB
Swedish Nuclear Power Inspectorate

UNITED KINGDOM
Central Electricity Generating Board
Nuclear Installations Inspectorate
United Kingdom Atomic Energy Authority

UNITED STATES OF AMERICA
Argonne National Laboratory
EG & G Idaho Falls
Electric Power Research Institute
Nuclear Regulatory Commission
Oak Ridge National Laboratories
12. REFERENCES


7. BAKER L and JUST L C. Studies of metal-water reactions at high temperatures. III. Experimental and theoretical studies of the zirconium-water reaction. ANL 6548. May 1962.


14. BROWN A F and HEALEY T. The kinetics of total oxygen uptake in steam-oxidised Zircaloy-2 in the range 1273-1673K. Journal of


29. YUREK G J, CATHCART J V and PAWEL R E. Microstructures of the scales formed on Zircaloy-4 in steam at elevated temperatures. Oxidation of Metals Vo. 10 No. 4 1976 255.


33. IGLESIAS F C. AECL unpublished work.


35. LEISTIKOW S, KRAFT R and POTT E. Is air a suitable environment for simulation of Zircaloy-steam high temperature oxidation within engineering experiments. EUR 6984, 1981.

36. WESTERMAN R E. High temperature oxidation of Zirconium and Zircaloy-2 and oxygen or water vapour HW-73511 1962.


42. HOMMA K et al. Behaviour of the Zircaloy cladding tube in a mixed gas of hydrogen and steam JAERI M 7131 1977. NRC translation


49. GRAIN C F and GARVIE R C. Mechanism of the monoclinic to tetragonal transformation of zirconium dioxide BUREAU OF MINES REPORT. BM-RI-6619, 1964.


54. PARSONS P D and HAND K. Springfields Nuclear Power Development Laboratories UKAEA unpublished work.


60. HOBSON D O and RITTENHOUSE P L. Embrittlement of Zircaloy-clad fuel rods by steam during LOCA transients. ORNL-4758, Jan. 1972.


69. CHUNG H M. Materials reactions accompanying degraded core accidents. Presented to Faculty Institute at the LWR degraded core cooling division of educational programme. ANL March 1981.


72. COURTWRIGHT E. BNWL Richland USA, Private communication.


74. MALANG S. SIMTRAN-1: A computer code for the simultaneous claculation of oxygen distribution and temperature profiles in Zircaloy during exposure to high temperature oxidising environments. ORNL-5083, Nov. 1975.

75. MALANG S and NEITZEL H J. Modelling of Zircaloy-steam-oxidation under severe fuel damage conditions OECD-NEA CSNI/IAEA Specialists meeting in water reactor fuel safety and fission produce release in off-normal and accident conditions Risø Denmark May 1983.


79. PAWEL R E. Oxygen diffusion in the oxide and alpha phases during reaction of Zircaloy-4 with steam from 1000° to 1500°C. J. Electrochem. Soc. 1979 126 (7) July 1111-1118.


84. DUNCAN D B. Moving boundaries in heat conduction and mass diffusion problems. AECL 8185, 1983.


86. BRZOSKA B et al. Parameter study on the influence of prepressurisation on PWR fuel rod behaviour during normal operating and hypothetical LOCAs. Nuclear Technology 1979 46 (2) December 205-212.

87. KARB E H et al. LWR fuel rod behaviour in the FR2 in-pile tests simulating the heatup phase of a LOCA KfK 3346 March 1983.


112. HASTE T J. Modelling the effect of different mechanical restraint conditions on PWR fuel rod deformation under conditions relevant to the NRU MT-3 experiment, using the MABEL 2-D code. UKAEA Report ND-R-988(S).


118. WIEHR K. Unpublished work.


126. KAWASAKI S. Private communication.


129. KAWASAKI S. Private communication.

130. KAWASAKI S. Private communication.

131. KAWASAKI S. Private communication.


133. UKAEA unpublished work.


136. HINDLE E D. Zircaloy fuel clad ballooning tests at 900-1070K in steam. UKAEA report ND-R-6(S), June 1977.


138. CHAPMAN R H. Preliminary multi-rod burst test program results and implications of interest to reactor safety evaluations. Paper


141. ERBACHER F J. Private communication.


144. CHAPMAN R H, LONGEST A W and CROWLEY J L. Experiment Data Report for Multirod Burst Test (MRBT) Bundle B-6, NUREG/CR-3460 (ORNL/TM-8890) (July 1984).


149. HEALEY T, CLAY B D and DUFFEY R B. Analysis of the axial ballooning behaviour of directly heated Zircaloy tubes. CEGB RD/B/N4145, Sept. 1977.

150 UKAEA Springfields unpublished work.


161. BORGWALDT H and GULDEN W. SSYST, a code-system for analysing transient LWR fuel rod behaviour under off-normal conditions. KFK 3359 June 1982.


171. KELPPE S. Private communication.


184. DONALDSON A T. Creep of α-phase Zircaloy-oxygen alloys containing 0 to 8 atomic per cent oxygen. CEGB RD/B/N4488, Jan. 1979.


188. HINDLE E D. Stress rupture and creep of Zircaloy-4 PWR cladding in the temperature range 875 to 915 K. UKAEA Report ND-R-191(S), Sept. 1978.


194. LEISTIKOW S. Comparison of High Temperature Steam Oxidation Kinetics under LWR Accident Conditions: Zircaloy-4 versus Austenitic Stainless Steel No. 1.4970. 6th International Conf. on Zirconium in the Nuclear Industry, June 28 to July 1, 1982. Vancouver, BC, Canada.

195. HOFMANN P and NEITZEL H J. External and internal reaction of Zircaloy tubing with Oxygen and UO2 and its modelling. 5th International Meeting on Thermal Nuclear Reactor Safety, Karlsruhe, Sept. 1983.


204. Broughton J et al. Effect of fuel re-distribution on cladding temperatures during a large break LOCA and impact on NRC licensing requirements. CSNI Meeting, Tokai Mura, Japan, 22 May 1982.
### Table 1

Temperature dependence of published isothermal parabolic rate constants

<table>
<thead>
<tr>
<th>Source</th>
<th>Temp. range °C</th>
<th>( \frac{\delta^2}{2} ) ( O_2/2 \ (\text{gm/cm}^2)^2/\text{s} )</th>
<th>( \frac{\delta^2}{2} ) ( H_2/2 \ (\text{STP cm}^3/\text{cm}^2)^2/\text{s} )</th>
<th>( K_p \ (\text{mg}2\text{r/cm}^2)^2/\text{s} )</th>
</tr>
</thead>
<tbody>
<tr>
<td>Leistikow et al(17)</td>
<td>1000-1300</td>
<td>0.262</td>
<td>174360</td>
<td>5.14 (10^5) As Column I</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td></td>
<td>42.58 (10^5) As Column I</td>
</tr>
<tr>
<td>Pawel et al(13)</td>
<td>1000-1500</td>
<td>0.1811</td>
<td>167190</td>
<td>3.55 (10^5) As Column I</td>
</tr>
<tr>
<td>ORNL</td>
<td></td>
<td></td>
<td></td>
<td>29.32 (10^5) As Column I</td>
</tr>
<tr>
<td>Kawasaki et al(12)</td>
<td>900-1300</td>
<td>0.234</td>
<td>170410</td>
<td>4.59 (10^5) As Column I</td>
</tr>
<tr>
<td>JAERI</td>
<td></td>
<td></td>
<td></td>
<td>38.03 (10^5) As Column I</td>
</tr>
<tr>
<td>Brown et al(14)</td>
<td>1000-1200 single oxide</td>
<td>0.1028</td>
<td>163190</td>
<td>2.02 (10^5) As Column I</td>
</tr>
<tr>
<td>BNL</td>
<td></td>
<td></td>
<td></td>
<td>16.71 (10^5) A Column I</td>
</tr>
<tr>
<td></td>
<td>1000-1400 double oxide</td>
<td>0.2238</td>
<td>174250</td>
<td>4.39 (10^5) As column I</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td></td>
<td>36.37 (10^5) As Column I</td>
</tr>
<tr>
<td>Biederman et al(9)</td>
<td>980-1480</td>
<td>0.0191</td>
<td>139690</td>
<td>3.74 (10^4) As Column I</td>
</tr>
<tr>
<td>WPI</td>
<td>650-816</td>
<td>5.73 (10^{-4})</td>
<td>114445</td>
<td>1.12 (10^3) As Column I</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td></td>
<td>9.32 (10^3) As Column I</td>
</tr>
<tr>
<td>Westerman et al(11)</td>
<td>970-1250</td>
<td>0.0321</td>
<td>145255</td>
<td>6.30 (10^4) As Column I</td>
</tr>
<tr>
<td>BNWL</td>
<td></td>
<td></td>
<td></td>
<td>5.22 (10^5) As Column I</td>
</tr>
<tr>
<td>Urbanic et al(15)</td>
<td>1050-1580</td>
<td>0.0182</td>
<td>139900</td>
<td>3.57 (10^6) As Column I</td>
</tr>
<tr>
<td>AECl</td>
<td>1580-1850</td>
<td>0.0541</td>
<td>138155</td>
<td>1.06 (10^5) As Column I</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td></td>
<td>8.79 (10^5) As Column I</td>
</tr>
<tr>
<td>Baker &amp; Just(7)</td>
<td>-1852</td>
<td>2.049</td>
<td>190465</td>
<td>4.02 (10^6) As Column I</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td></td>
<td>333 (10^5) As Column I</td>
</tr>
</tbody>
</table>
TABLE 2
Temperature dependence of parabolic growth constants for oxide and α-phase

<table>
<thead>
<tr>
<th></th>
<th>Oxide $8^2/2$ cm s$^{-1}$</th>
<th>α-phase $8^2/2$ cm s$^{-1}$</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>A</td>
<td>Q</td>
</tr>
<tr>
<td>Leistikow et al(17)</td>
<td>3.91 $10^{-2}$</td>
<td>168127</td>
</tr>
<tr>
<td>Pawel et al(13)</td>
<td>1.126 $10^{-2}$</td>
<td>150236</td>
</tr>
<tr>
<td>Beideman et al(9)</td>
<td>9.392 $10^{-4}$</td>
<td>122734</td>
</tr>
<tr>
<td>Kawasaki et al(12)</td>
<td>1.07 $10^{-2}$</td>
<td>150110</td>
</tr>
<tr>
<td>Urbanic &amp; Heidrick(15)</td>
<td>6.48 $10^{-4}$</td>
<td>113000</td>
</tr>
<tr>
<td>Sagat et al(16)</td>
<td>4.2 $10^{-2}$</td>
<td>169978</td>
</tr>
</tbody>
</table>
### TABLE 3

Summary of cladding oxidation parameters and performance limits for the ECCS in two PWRs for a double-ended guillotine break in the pump discharge leg (from 64)

**Evaluation of ECCS margin of performance**

<table>
<thead>
<tr>
<th>Reactor plant</th>
<th>Design basis accident</th>
<th>Clad oxidation parameters&lt;sup&gt;a&lt;/sup&gt;</th>
<th>Performance limits&lt;sup&gt;b&lt;/sup&gt;</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td>17% ECR, tf&lt;sub&gt;f&lt;/sub&gt;, ECR, L&lt;sub&gt;0.7&lt;/sub&gt;, L&lt;sub&gt;0.9&lt;/sub&gt;, 17% ECR, 0.3 Joule impact, L&lt;sub&gt;0.7&lt;/sub&gt;, Thermal shock, L&lt;sub&gt;0.9&lt;/sub&gt;</td>
<td></td>
</tr>
<tr>
<td></td>
<td></td>
<td>s, s, %, mm, mm, mm, 17% ECR, 0.3</td>
<td></td>
</tr>
<tr>
<td><strong>Two-side oxidation</strong></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>OS</td>
<td>0.8 DEGPLD</td>
<td>PCT</td>
<td>275</td>
</tr>
<tr>
<td>CF</td>
<td>1.0 DECLG</td>
<td>PCT</td>
<td>325</td>
</tr>
<tr>
<td></td>
<td></td>
<td>RUP</td>
<td>-</td>
</tr>
<tr>
<td><strong>One-side oxidation</strong></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>OS</td>
<td>0.8 DEGPLD</td>
<td>PCT</td>
<td>-</td>
</tr>
<tr>
<td>CF</td>
<td>1.0 DECLG</td>
<td>PCT</td>
<td>-</td>
</tr>
</tbody>
</table>

<sup>a</sup>Values computed from the oxidation model reported in ANL-79-48, NUREG/CR 1344

<sup>b</sup>Value of > 1 indicates performance limit is met
### TABLE 4

Hydrogen generation from the Zr/H$_2$O reaction from ref (73)

<table>
<thead>
<tr>
<th>T K</th>
<th>W$_{H_2}$/t kg s$^{-1}$</th>
<th>Time to produce 100 kg of H$_2$ (s)</th>
</tr>
</thead>
<tbody>
<tr>
<td>800</td>
<td>0.012</td>
<td>$10^7$</td>
</tr>
<tr>
<td>1000</td>
<td>0.15</td>
<td>$10^5$</td>
</tr>
<tr>
<td>1200</td>
<td>0.83</td>
<td>$10^4$</td>
</tr>
<tr>
<td>1400</td>
<td>2.8</td>
<td>$10^5$</td>
</tr>
<tr>
<td>1600</td>
<td>6.8</td>
<td>216</td>
</tr>
<tr>
<td>1800</td>
<td>13.9</td>
<td>52</td>
</tr>
<tr>
<td>2000</td>
<td>24.1</td>
<td>17</td>
</tr>
<tr>
<td>2200</td>
<td>37.0</td>
<td>7.3</td>
</tr>
</tbody>
</table>

### TABLE 5

FBR in-pile tests on fuel behaviour. Test matrix (87)

<table>
<thead>
<tr>
<th>Type of Test</th>
<th>Test Series</th>
<th>Number of Rods</th>
<th>Number of Tests</th>
<th>Target Burnup</th>
<th>Range of Internal Pressure at Steady State Temperature</th>
</tr>
</thead>
<tbody>
<tr>
<td>Calibration, Scoping</td>
<td>A</td>
<td></td>
<td>5</td>
<td>-</td>
<td>25 - 100</td>
</tr>
<tr>
<td>Unirradiated Rods</td>
<td>B'1</td>
<td>-</td>
<td>7</td>
<td>0</td>
<td>55 - 90</td>
</tr>
<tr>
<td>Main Parameters: Internal Pressure</td>
<td>B 3</td>
<td>-</td>
<td>2</td>
<td>0</td>
<td>55 - 90</td>
</tr>
<tr>
<td>Irradiated Rods</td>
<td>C</td>
<td>6</td>
<td>5</td>
<td>2500</td>
<td>25 - 110</td>
</tr>
<tr>
<td>Main Parameter: Burnup</td>
<td>E</td>
<td>6</td>
<td>5</td>
<td>8000</td>
<td>25 - 120</td>
</tr>
<tr>
<td></td>
<td>F</td>
<td>6</td>
<td>5</td>
<td>20000</td>
<td>45 - 85</td>
</tr>
<tr>
<td></td>
<td>G 1</td>
<td>6</td>
<td>5</td>
<td>35000</td>
<td>50 - 90</td>
</tr>
<tr>
<td></td>
<td>G 2</td>
<td>2</td>
<td>2</td>
<td>35000</td>
<td>60 - 125</td>
</tr>
<tr>
<td></td>
<td>G 3</td>
<td>4</td>
<td>3</td>
<td>35000</td>
<td></td>
</tr>
<tr>
<td>Electrically Heated Fuel Rod Simulators</td>
<td>BSS</td>
<td>-</td>
<td>8</td>
<td>-</td>
<td>20 - 110</td>
</tr>
<tr>
<td>Main Parameter: Internal Pressure</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
</tbody>
</table>
## Table 6  Summary of cladding deformation data from Tests LOC-3, LOC-5, and LOC-6

<table>
<thead>
<tr>
<th>Test</th>
<th>Rod Number</th>
<th>Initial Pressure (MPa)</th>
<th>Burnup (MWd/kt)</th>
<th>Maximum Circumferential Elongation (%)</th>
<th>Axial Extent of Deformation &gt; 5% (m)</th>
<th>Location Failure (m)</th>
<th>Failure Time (s)</th>
<th>Cladding Burst Temperature (°C)</th>
<th>Heating Rate (K/s)</th>
<th>Burst Pressure (MPa)</th>
</tr>
</thead>
<tbody>
<tr>
<td>LOC-3 1</td>
<td>2.45</td>
<td>0</td>
<td>29</td>
<td>0.2 to 0.6</td>
<td>0.257</td>
<td>15.0</td>
<td>1100 to 1200 (1190)</td>
<td>4.3</td>
<td>1.6</td>
<td></td>
</tr>
<tr>
<td>2</td>
<td>2.38</td>
<td>15990</td>
<td>40</td>
<td>0.5 to 0.6</td>
<td>0.543</td>
<td>7.9</td>
<td>1300 to 1530 (1300)</td>
<td>20.0</td>
<td>1.0</td>
<td></td>
</tr>
<tr>
<td>3</td>
<td>4.92</td>
<td>0</td>
<td>20</td>
<td>0.15 to 0.6</td>
<td>0.244</td>
<td>10.1</td>
<td>1105 to 1130 (1110)</td>
<td>15.0</td>
<td>5.1</td>
<td></td>
</tr>
<tr>
<td>4</td>
<td>4.75</td>
<td>16620</td>
<td>41.6</td>
<td>0.10 to 0.65</td>
<td>0.320</td>
<td>13.1</td>
<td>1110 to 1140 (1120)</td>
<td>15.0</td>
<td>4.8</td>
<td></td>
</tr>
<tr>
<td>LOC-5</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>6</td>
<td>2.41</td>
<td>17660</td>
<td>35</td>
<td>0.13 to 0.65</td>
<td>0.507</td>
<td>10.5</td>
<td>1300 to 1400 (1350)</td>
<td>0</td>
<td>0.6</td>
<td></td>
</tr>
<tr>
<td>7A</td>
<td>4.83</td>
<td>0</td>
<td>19</td>
<td>0.10 to 0.60</td>
<td>0.238</td>
<td>2.75</td>
<td>1130 to 1230 (1160)</td>
<td>100.0</td>
<td>3.5</td>
<td></td>
</tr>
<tr>
<td>7B</td>
<td>4.83</td>
<td>0</td>
<td>48</td>
<td>0.10 to 0.60</td>
<td>0.305</td>
<td>7.8</td>
<td>1300 to 1900 (1350)</td>
<td>70.0</td>
<td>0.7</td>
<td></td>
</tr>
<tr>
<td>LOC-6</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>9</td>
<td>2.41</td>
<td>0</td>
<td>&lt;1</td>
<td>--c</td>
<td>--c</td>
<td>--</td>
<td>--</td>
<td>--</td>
<td>--</td>
<td></td>
</tr>
<tr>
<td>10</td>
<td>2.41</td>
<td>10800</td>
<td>13.6</td>
<td>0.25 to 0.56</td>
<td>--c</td>
<td>--</td>
<td>--</td>
<td>--</td>
<td>--</td>
<td></td>
</tr>
<tr>
<td>11</td>
<td>4.74d</td>
<td>0</td>
<td>31</td>
<td>0.25 to 0.43</td>
<td>0.374</td>
<td>5.2</td>
<td>1010 to 1105 (1098)</td>
<td>100</td>
<td>14</td>
<td></td>
</tr>
<tr>
<td>12</td>
<td>4.83</td>
<td>10800</td>
<td>74</td>
<td>0.22 to 0.52</td>
<td>0.36</td>
<td>18.2</td>
<td>1010 to 1105 (1066)</td>
<td>0</td>
<td>5.3</td>
<td></td>
</tr>
</tbody>
</table>

---

a. Temperature in parentheses is the best estimate within the range estimated from the cladding microstructure.

b. This is the heating rate from the cladding surface thermocouples at 0.625 m above the bottom of the fuel.

c. Cladding did not fail.

d. The initial internal pressure was probably about 12 MPa, due to a coolant leak and subsequent formation of steam.
<table>
<thead>
<tr>
<th>DATA</th>
<th>EOLO 1</th>
<th>EOLO 2</th>
<th>EOLO 3</th>
<th>EOLO 4</th>
<th>EOLO 5</th>
</tr>
</thead>
<tbody>
<tr>
<td>Rod peak Linear power (kW/m)</td>
<td>4.32</td>
<td>4.44</td>
<td>3.93</td>
<td>4.32</td>
<td>3.80</td>
</tr>
<tr>
<td>Fuel Rod Cold Pressure $P_2$ (MPa)</td>
<td>4.82</td>
<td>4.79</td>
<td>4.9</td>
<td>4.9 1</td>
<td>5.0</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>$P_2-P_1$ at $t=0$ (MPa)</td>
<td>6.9</td>
<td>5.8</td>
<td>8.5 MAX</td>
<td>6.1</td>
<td>5.4</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>$P_2-P_1$ at t burst (MPa)</td>
<td>6.2</td>
<td>5.5</td>
<td>5.7</td>
<td>5.6</td>
<td>5.0</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Controlled Clad Temp. at $t = 0$ (K)</td>
<td>980</td>
<td>1056</td>
<td>1080</td>
<td>1016</td>
<td>970 990</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Peak clad temperature at burst time (K)</td>
<td>1075</td>
<td>1044</td>
<td>1110</td>
<td>1030</td>
<td>995</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Max $\Delta T$ during ballooning (K)</td>
<td>101</td>
<td>92</td>
<td>38</td>
<td>55</td>
<td>48</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Burst Time (sec)</td>
<td>200</td>
<td>130</td>
<td>10</td>
<td>372</td>
<td>3460 540</td>
</tr>
</tbody>
</table>
TABLE 8

REBEKA test philosophy and test objectives (115, 116)

- single rod tests
  - 325mm fuel rod simulator
  - steam atmosphere

  - cladding deformation mechanism
  - influence of circumferential cladding temperature difference on burst strain

  burst criterion

- 5x5 bundle tests
  - 3900mm fuel rod simulators
  - forced flooding

  - interaction between thermohydraulics and cladding deformation
  - influence of spacer grids on cladding deformation
  - influence of control rod guide thimbles on cladding deformation

- 7x7 bundle tests
  - 3900mm fuel rod simulators
  - forced flooding

  - rod to rod interaction and failure propagation
  - influence of coolant flow direction
    - flow blockage
  - flow blockage ratio

SSYST fuel rod behaviour code
<table>
<thead>
<tr>
<th>Test</th>
<th>Type of pressurised rod array</th>
<th>Thermal hydraulic conditions during deformation</th>
<th>Cladding strain max. circlmf. %</th>
<th>Max. flow blockage %</th>
<th>Comments</th>
</tr>
</thead>
<tbody>
<tr>
<td>R1</td>
<td>3 x 3</td>
<td>Single-phase then two-phase, counter current</td>
<td>32</td>
<td>25</td>
<td>Enhanced cooling at start owing to malfunction. Deformation occurred over 100 s.</td>
</tr>
<tr>
<td>R2</td>
<td>3 x 3</td>
<td>Single-phase</td>
<td>64</td>
<td>60</td>
<td>Repeat of R1 with correct cooling.</td>
</tr>
<tr>
<td>R3</td>
<td>3 x 3</td>
<td>Single-phase then two-phase, counter current</td>
<td>64</td>
<td>52</td>
<td>Repeat of R1 but including cold control rod guide tube.</td>
</tr>
<tr>
<td>R4</td>
<td>3 x 3</td>
<td>Single-phase then two-phase, counter current</td>
<td>79</td>
<td>55</td>
<td>Repeat of R1 but including cold control rod guide tube.</td>
</tr>
<tr>
<td>Materials</td>
<td>5 x 5</td>
<td>Single-phase</td>
<td>89</td>
<td>84</td>
<td>Steam flow and rod power not typical of calculated LOCA conditions.</td>
</tr>
<tr>
<td>R5</td>
<td>7 x 7</td>
<td>Single-phase then two-phase, counter current</td>
<td>75</td>
<td>52</td>
<td>Repeat of R3 with more rods pressurised</td>
</tr>
<tr>
<td>R6</td>
<td>7 x 7</td>
<td>Single-phase then two-phase, unidirectional flow</td>
<td>65</td>
<td>60</td>
<td>International standard problem 14</td>
</tr>
</tbody>
</table>
**TABLE 10**

*Design Parameter of a Fuel Bundle (JAERI) (132)*

<table>
<thead>
<tr>
<th>Description</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>No. of Rods</td>
<td>49/ass. (7x7)</td>
</tr>
<tr>
<td>Rod-Rod Span</td>
<td>14.3 mm</td>
</tr>
<tr>
<td><strong>Tubing</strong></td>
<td></td>
</tr>
<tr>
<td>Material</td>
<td>Zircaloy-4</td>
</tr>
<tr>
<td>Outer Dia.</td>
<td>10.72 mm</td>
</tr>
<tr>
<td>Inner Dia.</td>
<td>9.48 mm</td>
</tr>
<tr>
<td>Length</td>
<td>1569 mm</td>
</tr>
<tr>
<td><strong>Heater</strong></td>
<td></td>
</tr>
<tr>
<td>Material</td>
<td>W-3%Re</td>
</tr>
<tr>
<td>Diameter</td>
<td>0.6 mm (6 wires are arrayed in parallel)</td>
</tr>
<tr>
<td>Length</td>
<td>850 mm</td>
</tr>
<tr>
<td><strong>Insulator</strong></td>
<td></td>
</tr>
<tr>
<td>Material</td>
<td>Al₂O₃</td>
</tr>
<tr>
<td>Diameter</td>
<td>9.20 mm (each pellet has 6 holes)</td>
</tr>
<tr>
<td>Length</td>
<td>20 mm</td>
</tr>
<tr>
<td><strong>Internal Gas</strong></td>
<td></td>
</tr>
<tr>
<td></td>
<td>He</td>
</tr>
<tr>
<td><strong>Thermo-Couple</strong></td>
<td></td>
</tr>
<tr>
<td>Material</td>
<td>C-A, sheathed by 304 s.s.</td>
</tr>
<tr>
<td>Diameter</td>
<td>0.8 mm</td>
</tr>
<tr>
<td><strong>Spacer</strong></td>
<td></td>
</tr>
<tr>
<td>Material</td>
<td>Inconel 718</td>
</tr>
<tr>
<td>Number</td>
<td>2/ass.</td>
</tr>
<tr>
<td>Spacer-Spacer Span</td>
<td>650 mm</td>
</tr>
</tbody>
</table>

- 98 -
## TABLE 11

Test matrix of bundle burst tests using W-Re wire heaters and a close fitting shroud (JAERI) (132)

<table>
<thead>
<tr>
<th>Bundle No.</th>
<th>Steam Flow Rate (g/cm² min)</th>
<th>Heating Rate (°C/s)</th>
<th>Init. Int. Press. (kg/cm²)</th>
<th>Max. Int. Press. (kg/cm²)</th>
<th>Burst Temp. (°C)</th>
</tr>
</thead>
<tbody>
<tr>
<td>71005</td>
<td>0.44</td>
<td>6.6 - 8.7 (500 - 860°C)</td>
<td>50</td>
<td>64 - 70</td>
<td>805 - 860</td>
</tr>
<tr>
<td>71006</td>
<td>0.40</td>
<td>7.3 - 9 (500 - 900°C)</td>
<td>20</td>
<td>26 - 29</td>
<td>870 - 920</td>
</tr>
<tr>
<td>71007</td>
<td>0.40</td>
<td>5.9 - 7.2 (430 - 830°C)</td>
<td>70</td>
<td>87 - 93</td>
<td>750 - 790</td>
</tr>
<tr>
<td>71008</td>
<td>0.44</td>
<td>5.9 - 7.9 (500 - 880°C)</td>
<td>35</td>
<td>45 - 48</td>
<td>870 - 880</td>
</tr>
<tr>
<td></td>
<td>B-1</td>
<td>B-2</td>
<td>B-3</td>
<td>B-5</td>
<td></td>
</tr>
<tr>
<td>-----------------------------------</td>
<td>------</td>
<td>------</td>
<td>------</td>
<td>------</td>
<td></td>
</tr>
<tr>
<td>Bundle Heat Rate (°C/Sec)</td>
<td>29.5</td>
<td>28.5</td>
<td>9.5</td>
<td>9.8</td>
<td></td>
</tr>
<tr>
<td>Shroud Heat Rate (°C/Sec)</td>
<td>20.0</td>
<td>0</td>
<td>7.5</td>
<td>4.4</td>
<td></td>
</tr>
<tr>
<td>Inlet Steam Temperature (°C)</td>
<td>349</td>
<td>322</td>
<td>320</td>
<td>355</td>
<td></td>
</tr>
<tr>
<td>Inlet Steam Reynolds Number</td>
<td>250</td>
<td>290</td>
<td>263</td>
<td>—</td>
<td></td>
</tr>
<tr>
<td>Initial Temperature (°C)</td>
<td>355</td>
<td>334</td>
<td>329</td>
<td>335</td>
<td></td>
</tr>
<tr>
<td>Initial Pressure (KPa)</td>
<td>8680</td>
<td>8770</td>
<td>11610</td>
<td>11625</td>
<td></td>
</tr>
<tr>
<td>Maximum Pressure (KPa)</td>
<td>9100</td>
<td>9200</td>
<td>12110</td>
<td>12155</td>
<td></td>
</tr>
<tr>
<td>Burst Pressure (KPa)</td>
<td>7425</td>
<td>7560</td>
<td>9425</td>
<td>9425</td>
<td></td>
</tr>
<tr>
<td>Burst Temperature (°C)</td>
<td>865</td>
<td>857</td>
<td>764</td>
<td>775</td>
<td></td>
</tr>
<tr>
<td>Burst Time (sec)</td>
<td>17.0—17.6</td>
<td>17.8—18.3</td>
<td>44.5—47.6</td>
<td>46—49</td>
<td></td>
</tr>
<tr>
<td>Burst Strain (%)</td>
<td>32—59</td>
<td>34—58</td>
<td>42—77</td>
<td>45—75</td>
<td></td>
</tr>
<tr>
<td>Tube Volume Increase (%)</td>
<td>27—55</td>
<td>28—52</td>
<td>29—50</td>
<td>40—60</td>
<td></td>
</tr>
<tr>
<td>Tg — Tg at Burst Time (°C)</td>
<td>140</td>
<td>355</td>
<td>80</td>
<td>240</td>
<td></td>
</tr>
<tr>
<td>Maximum Flow Blockage (%)</td>
<td>49</td>
<td>54</td>
<td>77</td>
<td>90*</td>
<td></td>
</tr>
</tbody>
</table>

* Inner 4 x 4 array
<table>
<thead>
<tr>
<th>Test parameters</th>
<th>Single rod tests</th>
<th>Multi rod tests</th>
</tr>
</thead>
<tbody>
<tr>
<td>Initial Pressure</td>
<td>20-150 bar</td>
<td>50/65/80 bar</td>
</tr>
<tr>
<td></td>
<td></td>
<td>(Constant Pressure for Tests with Conv. Cooling)</td>
</tr>
<tr>
<td>Initial Temperature</td>
<td>620 K (350°C)</td>
<td>620 K (320°C)</td>
</tr>
<tr>
<td>Initial Free Volume</td>
<td>40 cm³</td>
<td>40 cm³</td>
</tr>
<tr>
<td></td>
<td></td>
<td>(Tests with Forced Cooling)</td>
</tr>
<tr>
<td>Temperature Range</td>
<td>970-1270 K (700-1000°C)</td>
<td>970-1170 K (700-900°C)</td>
</tr>
<tr>
<td>Heating Rate</td>
<td>2-28 K/s</td>
<td>6-25 K/s</td>
</tr>
<tr>
<td>Holding Time</td>
<td>&lt; 90 s</td>
<td>&lt; 90 s</td>
</tr>
<tr>
<td>External Atmosphere</td>
<td>Air/Forced Steam (5 kg/h)</td>
<td>Air/Forced Air</td>
</tr>
<tr>
<td></td>
<td></td>
<td>10-80 W/m²K</td>
</tr>
<tr>
<td>Pressurising Medium</td>
<td>Helium</td>
<td>Helium</td>
</tr>
</tbody>
</table>
FIG. 1. ZIRCALOY-2 OXIDISED IN STEAM AT 1400°C FOR 200s
FIG. 2  TEMPERATURE DEPENDENCE OF THE PARABOLIC RATE CONSTANT FOR THE OXIDATION OF ZIRCALOY IN STEAM
FIG. 3 TEMPERATURE DEPENDENCE OF THE PARABOLIC RATE CONSTANT FOR THE OXIDATION OF ZIRCALOY IN STEAM
FIG. 4 TEMPERATURE DEPENDENCE OF THE PARABOLIC RATE CONSTANT FOR THE OXIDATION OF ZIRCALOY IN STEAM
Fig. 5: Parabolic Oxide Layer Growth Rate Constants Obtained for Pure Steam Environments vs Inverse Temperature.
Fig 6
High-Temperature Steam Oxidation of Zircaloy-4 Cladding
Kinetics of \( \text{ZrO}_2 \)- Scale and \( \alpha\)-Zr(O)-Layer Growth (600-1600°C, 2min-25h) (19)
Fig. 7  
Breakaway-Effect Related Features During High Temperature  
Oxidation (1000°C) of Zircaloy 4 in Steam and  
Tentative Sequence of Causal Mechanisms  

1. Oxide layer formation  
   - $O_2^-$ ion transport (logistic growth)  
   - high defect concentration (substoichiometry)  
   - compressive growth stress  

2. Growth of tetragonal oxide  
   - coarsening of columnar grains  
   - recombination of point defects  
   - relaxation of compressive stress  

3. Transformation towards monoclinic oxide  
   - inhomogeneous start of martensitic transformation  
   - interdependence with defect- and stress-distribution  
   - local vacancy agglomeration, pore formation near growth front  

4. Locally retarded oxide growth  
   - barrier effect of monoclinic domains and pores  
   - embrittlement of oxide by oxygen saturation tendency  
   - formation of scalloped growth front  

5. Formation of lateral cracks  
   - stress relief by crack nucleation at porous positions  
   - interaction to first lateral crack system  
   - resumed growth of new tetragonal oxide sublayer  

6. Formation of vertical cracks  
   - interaction with existing lateral cracks  
   - accelerated oxygen uptake by molecular transport  
   - formation of stoichiometric tubular oxide, spalling  

7. Periodic repetition of mechanisms  
   - phase transformation, pore formation in next sublayer  
   - mechanical failure by additional lateral crack systems  
   - quasilinear or faster post-breakaway kinetics  

Exercise 22
Figure 8  Burnup Dependence of Corrosion (B1)
FIG. 9(a) OUTSIDE OXIDE THICKNESS FOR ISOTHERMAL OXIDATION OF 5 μm PREOXIDIZED ZIRCALOY-4 (FROM 10)

FIG. 9(b) OUTSIDE OXIDE THICKNESS FOR ISOTHERMAL OXIDATION OF 10 μm PREOXIDIZED ZIRCALOY-4 (FROM 10)
FIG. 10. Zr-4 HIGH TEMPERATURE OXIDATION IN VARIOUS GASES (17)
Fig. 11 Variation of total weight gain of oxidized specimen with hydrogen volume fraction in a steam-hydrogen mixture (43)

Fig. 12 Hydrogen content absorbed by specimens oxidized in a steam hydrogen mixture (43)
Fig. 13. Ratio of Parabolic Oxide Layer Growth Rate Constant Obtained in Hydrogen-Steam Mixtures to that Obtained in an Unlimited Flux of Pure Steam as a Function of Zircaloy-4 Temperatures and Steam Supply Rate. The static hydrogen overpressure was kept constant at ~36 kPa and the specimen was oxidized by steam molecules impinging the oxide surface through natural convection. (20)

Fig. 14. Ratio of Parabolic Oxide Layer Growth Rate Constant Obtained in Flowing Hydrogen-Steam Mixtures to that Obtained in an Unlimited Flux of Pure Steam as a Function of Zircaloy-4 Temperature and Average Hydrogen Mol Fraction in the Inlet Mixtures. Hydrogen input to the shrouded reaction channel was 0.446 mol/min and steam flow was varied 0.5-3.2 g/min. (20)
Fig. 15. Fraction of Hydrogen Atoms Dissolved in a Cross Section of Zircaloy-4 Cladding Tube Out of Total Hydrogen Atoms Produced as a Result of Zircaloy-Steam Reaction at Isothermal Oxidation Temperatures Between 1000 and 1700°C. The fractions from oxidation tests in pure steam were obtained from Ref. 44 and similar values for oxidation in hydrogen-steam mixtures were obtained from specimens described in association with Figs. 13 and 14 (20).
Fig. 16  Radial strain versus time for Zircaloy-4 cladding oxidised in steam

[18]
Fig. 17  Axial strain versus time for Zircaloy-4 cladding oxidised in steam (18)
FIG. 18. SNL AND USA EMbrittlement DATA
FIG. 19 THERMAL-SHOCK FAILURE MAP FOR ZIRCALOY-4 CLADDING
(BOTTOM FLOODED WITH WATER AT THE OXIDATION TEMPERATURE)
RELATIVE TO THE EQUIVALENT-CLADDING-REACTION PARAMETER
AND MAXIMUM OXIDATION TEMPERATURE AFTER RUPTURE IN STEAM.
THE BEST-ESTIMATE FAILURE BOUNDARY FOR CLADDING THAT WAS
SLOW COOLED THROUGH THE $\beta \rightarrow \alpha'$ TRANSFORMATION BEFORE FLOODING
WITH WATER AND THE DATA OF HESSON ET AL. AND SCATENA ARE
SHOWN FOR COMPARISON. (FROM 44)

FIG. 20 FAILURE MAP FOR ZIRCALOY-4 CLADDING BY THERMAL SHOCK
RELATIVE TO FRACTIONAL THICKNESS OF PREVIOUS $\beta$-PHASE
LAYER AND OXIDATION TEMPERATURE AFTER RUPTURE IN
STEAM AND FLOODING WITH WATER AT OXIDATION TEMPERATURE.
COOLING RATE THROUGH THE $\beta \rightarrow \alpha'$ TRANSFORMATION
WAS $\sim$ 100 K/s. (FROM 44)
FIG. 21
FAILURE MAP FOR ZIRCALOY-4 CLADDING BY THERMAL SHOCK RELATIVE TO FRACTIONAL SATURATION OF ß PHASE AND OXIDATION TEMPERATURE AFTER RUPTURE IN STEAM AND FLOODING WITH WATER AT OXIDATION TEMPERATURE. THE FAILURE BOUNDARY FOR CLADDING THAT WAS SLOW-COOLED (≈ 5K/s) THROUGH THE PHASE TRANSFORMATION IS SHOWN FOR COMPARISON. (FROM 44)

FIG. 22
FAILURE MAP FOR ZIRCALOY-4 CLADDING BY THERMAL SHOCK RELATIVE TO THE WALL THICKNESS WITH ≤ 0.9 wt% OXYGEN AFTER ISOTHERMAL OXIDATION AND FLOODING WITH WATER AT THE OXIDATION TEMPERATURE. COOLING RATE THROUGH THE ß→α TRANSFORMATION WAS ≈ 100 K/s. (FROM 44)
Fig. 23

Capability of Zircaloy-4 Cladding to Withstand an Impact Energy of 0.3 J at 300 K Relative to the Thickness of 8-phase Layer Containing < 0.7 wt % Oxygen and the Hydrogen Content of the Cladding. (47)

Fig. 24

Time-Temperature Oxidation Conditions Resulting in Integrated Energies to Maximum Load above and below 0.3 J from Load-vs-Deflection Curves during Slow Diametral Compression of Tube and Ring Specimens (42) at 300 and 373 K, Respectively. (47)
Figure 25(a) Failure map for zircaloy-4 cladding by thermal shock relative to wall thickness with ≤0.9 wt% oxygen.

Figure 25(b) Failure map for zircaloy-4 cladding due to handling relative to wall thickness with <0.7 wt% oxygen.
a. ZIRCALOY TUBE UNDER BIAXIAL STRESS FROM INTERNAL GAS PRESSURE.

b. WHEN THE TUBE BULGES UNIFORMLTY MATERIAL FLOWS AXIALLY TO ACCOMMODATE THE BULGING AND THE TUBE SHORTENS.

c. NON-UNIFORM BULGING CAUSES THE HOT SIDE TO SHORTEN AND BOW AGAINST THE HEATER.

FIG. 26  SCHEMATIC REPRESENTATION OF STRAIN ANISOTROPY IN ZIRCALOY TUBING RESULTING IN THE "HOT-SIDE STRAIGHT" EFFECT.
Figure 27 Typical temperature and pressure histories; measured data of test B 3.1 (FR2 REF 87)
Figure 26 Relative increase of total void volume versus maximum circumferential elongation (26a) and deformation profiles for tests below the average (26b), above the average (26c), and tests F1 through F5 representing the average (26d). (REF 87)
Fig. 29: Posttest neutron radiograph of test-E5 fuel rod
Figure 30
Maximum circumferential elongation vs. burst temperature (FR2 REF 87)
Fig. 31
Temperature and internal pressure histories during test E4. (FR-2 Ref 87)
FIG. 32 COMPARISON OF THE AXIAL PROFILES OF CLADDING CIRCUMFERENTIAL STRAIN ON THE HIGH PRESSURE FRESH AND IRRADIATED RODS, RODS 11 AND 12, WHICH BURST IN THE ALPHA-PHASE (LOC TESTS IN PBF).\(^\text{[105]}\)

FIG. 33 COMPARISON OF THE AXIAL PROFILES OF CLADDING CIRCUMFERENTIAL STRAIN ON THE HIGH PRESSURE FRESH AND IRRADIATED RODS, ROD 3 AND 4, WHICH BURST IN THE ALPHA-PLUS-BETA TRANSITION (LOC TESTS IN PBF).\(^\text{[105]}\)
FIG. 34  COMPARISON OF THE AXIAL PROFILES OF CLADDING CIRCUMFERENTIAL STRAIN ON THE UNIRRADIATED HIGH PRESSURE RODS, RODS 7A AND 7B WITH INITIALLY UNDEFORMED AND COLLAPSED CLADDING (LOC TESTS IN PBFT,1105)
FIG. 35  TEST PROCEDURE FOR EOLO TESTS (106)
FIG. 36 POST IRRADIATION DIAMETRICAL METROLOGY OF THE FIVE BURST CLADDINGS (EOLO)\(^{106}\)
FIG. 37  EOLO 2 EVOLUTION OF THE CLADDING TEMPERATURES AT THE LEVEL 0.65 m, COOLANT FLOW AND ROD FILL GAS PRESSURE DURING THE TEST (106)

FIG. 38  EOLO-2 CLAD TEMPERATURE AND NEUTRON FLUX AXIAL PROFILES DURING THE TEST (106)
Figure 39
Position of MT assembly in NRU core

Figure 40
Schematic of NRU Loss-of-Coolant Accident Test Assembly
Fig 41a: Axial distribution of strain in NRU MT-1 & MT-2 tests
Fig 4.1b: Axial distribution of strain in NRU MT-3 & MT-4 tests
Fig. 42: Test Fuel Rod (PHEBUS) (ref)
Fig. 43: Measured Cladding Temperature Between Two Grid Spacers - Test 215 P
(L2, TC28, Rod 9), (L3, TC21, Rod B), (L4, TC22, Rod 7)
Measured Fuel Centerline Temperature (L2-TC44-Rod 9)
Fig. 44: Axial Deformation Profile Between the Interior Spacer Grids and Flow Area Restriction vs. Distance From Bottom of UO₂ Stack (mm) (102)

Fig. 45: Comparison of Maximum Burst Strains with Radial Position (Low Internal Pressure for Rods 2, 4 and 6) (102)
FIG. 48  FUEL ROD SIMULATOR (93) (KIK)
FIG. 49 TEST LOOP FOR BALLOONING EXPERIMENTS (114)
BUNDLE TESTS (REBEKA)
FIG. 50  REBEKA OUT-OF-PILE TESTS
BURST STRAIN VS. AZIMUTHAL TEMPERATURE DIFFERENCE. (90)
FIG. 51 BURST STRAIN VS. BURST TEMPERATURE OF ZIRCALOY CLADDINGS. (90)
- Starting Temperature 520°C
- Internal Rod Pressure 70 bar
- Decay Heat Rating at Midpoint 20 W/cm
- Heat Transfer Coefficient in Refill-Phase \( \sim 30 \text{ W/m}^2\text{K} \)
- Cladding Temperature at Start of Flooding 760–790°C
- Flooding Rate, Cold \( \sim 3 \text{ cm/s} \)
- Flood Water Temperature 130°C
- System Pressure 4 bar

**FIG. 52**  FIRST BUNDLE TEST (REBEKA)  
TEST DATA AND TEST PROCEDURE (93)
AXIAL DISTRIBUTION OF RATING IN FUEL SIMULATOR

FIG. 53 CIRCUMFERENTIAL STRAIN OF THE 9 ZIRCALOY CLADDINGS AND COOLANT CHANNEL BLOCKAGE (115,116)
FIG. 54  FIRST BUNDLE TEST AXIAL CLADDING TEMPERATURE DISTRIBUTION OF ROD 16 BETWEEN THE TWO CENTRAL SPACER GRIDS (93) (REBEKA)
FIG. 55 CIRCUMFERENTIAL STRAIN OF THE 9 RODS, AND COOLANT CHANNEL BLOCKAGE IN REBEKA 3 AND 4 (90 115 116)
FIG. 56 REBEKA 3 AXIAL DEFORMATION PROFILE OF ZIRCALOY CLADDINGS

FIG. 57 REBEKA 2 AXIAL TEMPERATURE DISTRIBUTION
FIG 58 REBEKA 5
axial deformation profile of the inner 25 rods

max. flow blockage: 52%
axial plane: 2000 mm from top.

FIG. 59. REBEKA 5 BUNDLE CROSS-SECTION AXIAL LEVEL OF MAX. FLOW BLOCKAGE (REF. 119)
Distance from upper end of heated zone (cm)

Fig. 60  REBEKA 6  Deformation of inner 18 rods (omitting rods with localised ballooning)
Fig. 61  REBEKA 6  Coolant channel blockage of inner 18 rods (omitting rods with localised ballooning)
FIG. 62 DOUBLE ENDED COLD LEG BREAK, PRESSURE DIFFERENCE ACROSS THE CLADDING AND CLADDING TEMPERATURE AT THE HOT SPOT.\(^{119}\)

FIG. 63 CONTROLLED TEMPERATURE TRANSIENT TEST.\(^{121}\)

FIG. 64 CREEP RUPTURE TEST (2nd PEAK SIMULATION)\(^{121}\)
Fig. 65: Burst Temperature vs. Pressure from Single Rod Tests Under Idealized Conditions. Comparison of Different Initial Cladding Material Conditions.

Fig. 66: Burst Strain vs. Burst Temperature from Single Rod Tests Under Idealized Conditions. Comparison of Different Cladding Material Conditions.
Fig. 67 Burst Strain vs. Wall Thickness Variation from Single Rod Creep Rupture Tests Under Idealized Conditions.

Fig. 68 Burst Strain vs. Mean Normalized Azimuthal Temperature Difference from Single Rod Creep Rupture Tests Under Idealized Conditions.
Fig 69: Directly Heated Single Rod Creep-Rupture Tests in Air
Tube Bending vs. Mean Normalized Azimuthal Temp Difference

Fig 70: Local Axial Strain vs. Tangential Strain in the Plane of Maximum Deformation.
Fig 71: Axial Strain vs. Tangential Strain for Creep Rupture Tests with Nonhomogeneous Temperature Distribution on the Circumference.

Fig 72: Bow versus azimuthal temperature difference.
**Figure 73** Axial Stress Profile for Transient Test (5Kg/s, 50 bar) in Air and Flowing Steam (5Kg/h)

**Figure 74** Indirectly Heated Multirod Test (3x4) with One Isobarically Pressurized Sample (65 bar) (KU)

<table>
<thead>
<tr>
<th>Sample 1</th>
<th>Sample 2</th>
</tr>
</thead>
<tbody>
<tr>
<td>2 bar</td>
<td>65</td>
</tr>
<tr>
<td>0.5 kg/s</td>
<td>8.2</td>
</tr>
<tr>
<td>0°C</td>
<td>820</td>
</tr>
<tr>
<td>E/ %</td>
<td>66</td>
</tr>
</tbody>
</table>

**Figure 75** Indirectly Heated Multirod Test (3x4) with One Isobarically Pressurized Sample (65 bar) (KU)

<table>
<thead>
<tr>
<th>Sample 1</th>
<th>Sample 2</th>
</tr>
</thead>
<tbody>
<tr>
<td>2 bar</td>
<td>65</td>
</tr>
<tr>
<td>0.5 kg/s</td>
<td>8.2</td>
</tr>
<tr>
<td>0°C</td>
<td>820</td>
</tr>
<tr>
<td>E/ %</td>
<td>66</td>
</tr>
</tbody>
</table>
Fig. 76a: Schematic Diagram of the Experimental Setup for KWU Multi Rod Tests with Forced Air Cooling.

Axial Temperature Profile

Axial Strain Profile

Fig. 76b: Axial Temperature and Strain Profile from KWU Multi Rod Test with Forced Air Cooling.
Fig 77: Burst Strain vs. Burst Temperature from Single Rod and Multi Rod Tests (Schematically).

Fig. 78 Schematic Diagram of a Facility for Burst Test of a Fuel Assembly (JAERI)
FIG. 79 SCHEMATIC DRAWINGS OF THE FUEL RODS AND THE HEATING SYSTEMS (126) (JAERI)
FIG. 80  LOCATIONS OF THERMOCOUPLES (JAERI) (126)
FIG. 82. HISTORIES OF CLADDING TEMPERATURES, INTERNAL PRESSURES AND OPERATING CONDITIONS (JAERI)
FIG. 83  HORIZONTAL TEMPERATURE DISTRIBUTION OF THE CLADDING AS A FUNCTION OF TIME  (DIAGONAL DIRECTION) (JAERI) (126)
FIG. 84. THE SIDE VIEWS OF A FUEL BUNDLE BURST AND CUT VERTICALLY (JAERI)\textsuperscript{(126)}
(BURST TEMP. 790-860°C)
ASS. No. 7805
FIG. 85. THE SIDE VIEWS OF A FUEL BUNDLE BURST AND CUT VERTICALLY (JAERI)\textsuperscript{(126)}
(BURST TEMP. 850–920\textdegree C)
ASS. No. 7806
FIG. 86. THE SIDE VIEWS OF A FUEL BUNDLE BURST AND CUT VERTICALLY. (JAERI)\textsuperscript{(126)}
(BURST TEMP. 740 - 790°C)
ASS. No. 7807
FIG. 87. SIDE VIEWS OF A FUEL BUNDLE BURST AND CUT VERTICALLY (JAERI)\textsuperscript{126} (BURST TEMP. 830-880°C) ASS. No. 7808
FIG. 88  AXIAL LOCATIONS OF BURST POSITION BALLOONED REGION
ASS. NO 7805 (JAERI)
FIG. 90 AXIAL LOCATIONS OF BURST POSITION BALLOONED REGION
ASS. NO 7807 (JAERI)\textsuperscript{126}
FIG. 92 AXIAL DISTRIBUTION OF BALLOONING IN JAERI 7 X 7 MULTIROD TESTS
FIG. 93 BURST LOCATIONS AND BURST TIME (JAERI)\(^{(126)}\)
FIG. 94  LOCATIONS OF THERMOCOUPLES. JAERI TESTS 9-14 (126)
FIG. 95  HISTORIES OF CLADDING TEMPERATURES INTERNAL PRESSURE AND OPERATING CONDITIONS WITH DEFORMING 7 X 7 ARRAY SURROUNDED BY SUB-HEATERS. (JAERII)\(^{(126)}\)
FIG. 96 AXIAL LOCATIONS OF BURST POSITION BALLOONED REGION
ASS. NO 7910 (JAERI) (126)
FIG. 97 AXIAL LOCATIONS OF BURST POSITION BALLOONED REGION
ASS. NO 7911 (JAERI) [126]
FIG. 98  AXIAL LOCATIONS OF BURST POSITION BALLOONED REGION
ASS. NO 7912 (JAERI)
<table>
<thead>
<tr>
<th></th>
<th>1</th>
<th>2</th>
<th>3</th>
<th>4</th>
<th>5</th>
<th>6</th>
<th>7</th>
</tr>
</thead>
<tbody>
<tr>
<td>A</td>
<td>65</td>
<td>145</td>
<td>130</td>
<td>175</td>
<td>175</td>
<td>155</td>
<td>60</td>
</tr>
<tr>
<td></td>
<td>58.70</td>
<td>58.74</td>
<td>60.76</td>
<td>60.74</td>
<td>46.74</td>
<td>57.70</td>
<td></td>
</tr>
<tr>
<td>B</td>
<td>130</td>
<td>80</td>
<td>95</td>
<td>220</td>
<td>55</td>
<td>170</td>
<td>175</td>
</tr>
<tr>
<td></td>
<td>52.76</td>
<td>56.78</td>
<td>52.78</td>
<td>58.78</td>
<td>60.76</td>
<td>52.74</td>
<td>50.74</td>
</tr>
<tr>
<td>C</td>
<td>40</td>
<td>NON-HEATING</td>
<td>230</td>
<td>240</td>
<td>NON-HEATING</td>
<td>225</td>
<td>150</td>
</tr>
<tr>
<td></td>
<td>57.76</td>
<td>54.80</td>
<td>56.80</td>
<td>58.78</td>
<td>46.72</td>
<td></td>
<td></td>
</tr>
<tr>
<td>D</td>
<td>75</td>
<td>35</td>
<td>140</td>
<td>150</td>
<td>150</td>
<td>190</td>
<td>35</td>
</tr>
<tr>
<td></td>
<td>48.76</td>
<td>48.76</td>
<td>50.80</td>
<td>48.76</td>
<td>50.78</td>
<td>52.78</td>
<td>76.110</td>
</tr>
<tr>
<td>E</td>
<td>170</td>
<td>35(40)</td>
<td>NON-HEATING</td>
<td>25</td>
<td>190</td>
<td>195(15)</td>
<td>80</td>
</tr>
<tr>
<td></td>
<td>58.76</td>
<td>50.76</td>
<td>64.78</td>
<td>48.80</td>
<td>50.78</td>
<td></td>
<td>58.76</td>
</tr>
<tr>
<td>F</td>
<td>65</td>
<td>190</td>
<td>30</td>
<td>130</td>
<td>NON-HEATING</td>
<td>40</td>
<td>120</td>
</tr>
<tr>
<td></td>
<td>56.72</td>
<td>56.78</td>
<td>58.78</td>
<td>58.80</td>
<td>50.76</td>
<td>50.72</td>
<td></td>
</tr>
<tr>
<td>G</td>
<td>80</td>
<td>70</td>
<td>45</td>
<td>60</td>
<td>55</td>
<td>140</td>
<td>155</td>
</tr>
<tr>
<td></td>
<td>50.70</td>
<td>50.72</td>
<td>56.72</td>
<td>56.76</td>
<td>54.76</td>
<td>50.72</td>
<td>48.72</td>
</tr>
</tbody>
</table>

Numeral in the parenthesis denotes an axial length which ballooned more than 34% at another location.

* Applied power was insufficient because of trouble in the power supply.

**Fig. 99** Axial length of ballooned region and times at burst and maximum pressure (JAERI) (126)
FIG. 100  PORTION OF TUBES WITH GREATER THAN 34% STRAIN.

(● NON-HEATED ROD, ○ MEASURED ROD) (JAERI(129))
FIG. 101 INCREASE IN TOTAL CROSSSECTIONAL AREA OF RODS AS A FUNCTION OF AXIAL LEVEL (JAERI)
(a) ILLUSTRATED DEFORMATION MECHANISM.

AVERAGE TEMPERATURES OF GROUPS IN NOS. 7912 AND 7914 AT FIRST BURST

<table>
<thead>
<tr>
<th>GROUP</th>
<th>SYMBOL</th>
<th>7912</th>
<th>7914</th>
</tr>
</thead>
<tbody>
<tr>
<td>NON-HEATED ROD</td>
<td>W</td>
<td>821.4k</td>
<td>889.6k</td>
</tr>
<tr>
<td>NEAREST NEIGHBOUR</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>COLD SIDE</td>
<td>X</td>
<td>1049.6</td>
<td>1032.6</td>
</tr>
<tr>
<td>HOT SIDE</td>
<td>Y</td>
<td>1078.1</td>
<td>1079.8</td>
</tr>
<tr>
<td>2nd NEAREST NEIGH.</td>
<td>Z</td>
<td>1091.6</td>
<td>1074.1</td>
</tr>
<tr>
<td>Z-Y</td>
<td></td>
<td>13.0</td>
<td>-5.7</td>
</tr>
</tbody>
</table>

(b) CLASSIFICATION OF RODS

FIG. 103 ILLUSTRATION OF THE DEFORMATION MECHANISM IN A BUNDLE AND AVERAGE TEMPERATURES OF ROD GROUPS AT THE FIRST ROD BURST. (JAERI) (128)
FIG. 104. CROSS SECTION OF JAERI TEST ARRAY WITH CONTROL ROD GUIDE TUBES
Fig. 105
AXIAL DISTRIBUTION OF CHANNEL BLOCKAGE.
Fig. 106
AXIAL DISTRIBUTION OF CHANNEL BLOCKAGE.
FIG. 107. CROSS-SECTION OF MAXIMUM BLOCKAGE IN JAERI TEST No. 24
FIG. 108 MRBT FUEL PIN SIMULATOR (ORNL)
FIG. 109  AVERAGE RUPTURE STRAIN FROM SINGLE-ROD TESTS, HEATED AT 28°C/s (135) (ORNL)

FIG. 110  CHANGE IN ZIRCALOY HEATED LENGTH IN SINGLE-ROD TESTS. (ORNL) (135)
FIG. 111 COMPARISON OF BURST STRAIN IN CREEP RUPTURE AND LOW HEATING RATE TESTS WITH DATA FOR 20°C/sec TESTS.\[137]\(\text{ORNL}\)

FIG. 112 COMPARISON OF TUBE VOLUME INCREASE (QUALITATIVELY EQUIVALENT TO AVERAGE TUBE STRAIN) IN CREEP RUPTURE AND LOW HEATING RATE TESTS WITH DATA FOR 20°C/sec TESTS.\[137]\(\text{ORNL}\)
FIG. 113 SCHEMATIC OF B-1 TEST ASSEMBLY (ORNL)

- SEAL GLANDS WITH ELECTRICAL, THERMOCOUPLE & PRESSURE PENETRATIONS
- BUNDLE & SHROUD CURRENT OUT (4)
- THERMAL BAFFLE (2)
- SUPERHEATED STEAM INLET
- STEAM OUTLET
- 95 mm
- 250 mm
- 915 mm HEATED LENGTH
- 1725 mm
- 560 mm
- 105 mm
- 210 mm
- THERMAL INSULATION
- HEATED SHROUD
- INSULATED GRID (3)
- FUEL PIN SIMULATORS
  10.9 mm O.D. TUBES ON 14.4 mm PITCH
- VESSEL HEATERS (TYPICAL)
- TEST VESSEL (305 mm I.D.)
- UNINSULATED GRID
- SEAL GLAND & CERAMIC INSULATOR
- SHROUD POWER LEADS (FLEXIBLE)
- SHROUD TRIM CURRENT (4)
- BUNDLE POWER LEADS (FLEXIBLE)
- BUNDLE CURRENT COLLECTOR

- 194 -
FIG. 114 COMPARISON OF BURST STRAINS IN B-1, B-2 AND B-3 TESTS WITH SINGLE-ROD TEST DATA (ORNL)
FIG. 115 COMPARISON OF B-3 BUNDLE BURST DATA WITH ORNL AND KFK SINGLE ROD DATA FOR HEATING RATE OF ~ 10 K/s (ORNL)

FIG. 116 PORTIONS OF TUBES WITH GREATER THAN 32% STRAIN IN B-3 TEST (78) (ORNL)
Note: The same fuel simulators were used for both tests, in the same positions and orientations.

FIG. 117 AXIAL DISTRIBUTION OF BURSTS IN MULTI-ROD BURST TESTS AT ORNL (138)
• STEAM FLOW WAS LOWER IN B-1 AND B-2 THAN IN SINGLE ROD TESTS
  B-1 RE ≈ 250; B-2 RE ≈ 290; SR RE 600-800
• SOME BURSTS IN B-1 (4) AND B-2 (8) ABOVE UPPER GRID BUT IN AGREEMENT
  WITH IR-SCAN
• HEATER FROM B-2 ROD 4 TESTED IN TWO SINGLE ROD TESTS WITH GRIDS
  • ONE AT STEAM FLOW NORMALLY USED IN SR TESTS
  • ONE AT STEAM FLOW SOMewhat LOWER THAN B-2 TEST
• RESULTS
  • BURST OCCURRED BELOW GRID WITH NORMAL STEAM FLOW
  • BURST OCCURRED ABOVE GRID WITH LOW STEAM FLOW
  • STEAM TEMPERATURE MEASUREMENTS SHOW SHORT-TERMA2H AL ENTRANCE
  • ZONE FOR LOW RE
• CONCLUSIONS:
  • LOW STEAM FLOW COMBINED WITH CHARACTERISTICS OF HEATERS CAUSED
    BURSTS TO OCCUR ABOVE UPPER GRID IN B-1 AND B-2
  • BURST LOCATIONS CAN BE LOWERED (AND POSSIBLY CLUSTERED) IN
    BUNDLES BY INCREASING THE STEAM FLOW
  • DEFORMATION NOT SENSITIVE TO STEAM FLOW RATE FOR 200 < RE < 300
  • CONVEXTIVE HEAT LOSSES TO STEAM BECOME MORE IMPORTANT ON TUBE
    TEMPERATURE PROFILE WITH DECREASING HEATING RATES

![Graph showing burst location and strain measurements.]

FIG. 118 BURST LOCATION CAN BE DISPLACED BY CHANGE IN STEAM FLOW (ORNL) (138)
FIG. 119  DEFORMATION AGREES WITH AXIAL TEMPERATURE DISTRIBUTION (ORNL)

FIG. 120  COMPARISON OF B-1 AND B-2 BUNDLE DATA WITH ORNL AND KFK SINGLE ROD DATA FOR HEATING RATE OF ~ 29 K/s (ORNL)
FIG. 121 RADIAL TEMPERATURE DISTRIBUTION BASED ON ROW- AND COLUMN-AVERAGED DATA 43s AFTER POWER-ON, FOR B-5 RODS. [127] (ORNL)
FIG. 122  SIMULATOR AVERAGE TEMPERATURE 43s AFTER POWER-ON IN B-5 TEST. (127) (ORNL)
FIG. 123 APPROXIMATE ORIENTATION OF BURSTS IN B-5 TEST. EXCEPT FOR NO. 62, WHICH WAS UNPRESSURIZED, SIMULATORS WITHOUT INDICATIONS RETAIN HEATERS, AND DETERMINATION OF BURST ORIENTATIONS MUST AVOID DESTRUCTIVE EXAMINATION. NOW DONE AND SHOWS PREDOMINANCE OF BURSTS TOWARDS THE FLOW CHANNEL AND TO N.E. CORNER.\[143] ORNL
FIG. 124  SMALL UNCONSTRAINED BUNDLES DO NOT PRODUCE SAME DEFORMATION PATTERNS AS LARGE
CONSTRAINED BUNDLES FOR SAME TEST CONDITION[14,3]

(PHOTOGRAPH REPRODUCED BY KIND PERMISSION OF OAK RIDGE NATIONAL LABORATORY)
FIG. 125  TUBE DILATATION GREATER IN B-5 INTERIOR SIMULATORS THAN IN EXTERIOR SIMULATORS AND GREATER THAN IN COMPARABLE B-3 (4 X 4) TEST (143) (ORNL)
FIG. 126 Subdivision of B-5 data shows interior subarrays have greater coolant flow area restriction (Ref. 127) (ORNL).

FIG. 127 Preliminary B-5 data show inner 4 x 4 array has greater flow restriction than B-3 (Ref. 127) (4 x 4) array (ORNL).
FIG. 128  PRELIMINARY DATA SHOW B-5 BURST STRAINS NOT A STRONG FUNCTION OF ROD POSITION AND ONLY SLIGHTLY GREATER THAN B-3 BURST STRAINS [14,3 (ORNL)]
FIG. 129  BURST STRAIN vs INTERNAL PRESSURE
WESTINGHOUSE SINGLE ROD BURST TEST (145)
FIG. 130 WESTINGHOUSE MULTI-ROD BURST TEST RESULTS (145)
FIG. 131
MAXIMUM CIRCUMFERENTIAL STRAIN AS A FUNCTION OF BURST TEMPERATURE FOR AXIALLY CONSTRAINED ZIRCALOY-4 CLADDING IN STEAM AND VACUUM ENVIRONMENTS AT HEATING RATE OF 5°C/s.\textsuperscript{146}(ANL)

FIG. 132
MAXIMUM CIRCUMFERENTIAL STRAIN VS BURST TEMPERATURE FOR UNCONSTRAINED AND MANDREL- AND PELLET-CONSTRAINED ZIRCALOY-4 CLADDING TUBES BURST IN STEAM AT HEATING RATE OF 5°C/s.\textsuperscript{146}(ANL)

FIG. 133
EFFECT OF INITIAL INTERNAL PRESSURE AND HEATING RATE IN STEAM ON MAXIMUM CIRCUMFERENTIAL STRAIN FOR ZIRCALOY-4 CLADDING CONSTRAINED BY PELLETS.\textsuperscript{146}(ANL)
SPECIMENS HEATED AT 10K/s IN STEAM TO SET TEMPERATURES AND THE CENTRE HELD AT CONSTANT (CONTROL) TEMPERATURE UNTIL RUPTURE. THE RUPTURE TIMES IN SECONDS ARE GIVEN BESIDE THE SYMBOLS AND THE MAXIMUM CIRCUMFERENTIAL STRAINS BELOW THEM.

- LOCALISED DEFORMATION
- 'CARROT'-SHAPED DEFORMATION
- UPPER LIMIT FOR EXTENDED DEFORMATIONS UNDER RADIATIVE COOLING.
- UPPER LIMIT FOR 'CARROT'-SHAPED DEFORMATIONS UNDER CONVECTIVE COOLING

FIG. 134 RESULTS OF STYLISED TRANSIENT TESTING OF ZIRCALOY-4 PWR CLADDING WITH MAINLY CONVECTIVE COOLING
FIG. 135 PROCESSES CONTROLLING STRAIN AND ITS STABILISATION
**Fig. 136** Variation of tube rupture strain with rupture temperature during isothermal tests at Springfields Nuclear Laboratories.

**Isothermal Tests in Centre of Two Phase Region**

<table>
<thead>
<tr>
<th>Hoop Stress (MN/m²)</th>
<th>Rupture (s)</th>
</tr>
</thead>
<tbody>
<tr>
<td>3.6</td>
<td>5366</td>
</tr>
<tr>
<td>5.6</td>
<td>1180</td>
</tr>
<tr>
<td>8.2</td>
<td>207</td>
</tr>
<tr>
<td>10.2</td>
<td>105</td>
</tr>
<tr>
<td>20.4</td>
<td>33</td>
</tr>
<tr>
<td>30.6</td>
<td>27</td>
</tr>
</tbody>
</table>

**Isothermal Tests at Start of Two Phase Region**

- 35.7
- 40.8
- 45.9
FIG. 137. X-RADIOPHAPHS OF BALLOONED IRRADIATED ZIRCALOY CLAD UO₂ FUEL ROD

PRINTS FROM RADIOPHAPHS OF RESIN-FILLED REGION OF TEST-PIECE 18/1.
ARROW INDICATES POSITION OF SPARK-ERODED HOLE

BOTTOM END

0°  120°  240°
Fig. 138 Mechanical restraint by deformable neighbours. CANSELW-2 idealisation compared with experimental cross-sections. (a) CANSELW-2 model for an array with rods straining at different rates; (b) Springfields 4x4 cladding deformation rig cross-sections.
\( P = \text{rod pitch} \)
\( A_0 = \text{original (unstrained) cross-sectional area of rod} \)
\( A_f = \text{final (strained) cross-sectional area of rod} \)
\[
B_1(\%) = \frac{(A_f - A_0)}{(p^2 - A_0)} \times 100 \quad \text{for one rod (local blockage)}
\]
\[
B(\%) = \frac{100}{n} \sum_{i=1}^{n} \frac{(A_f - A_0)}{(p^2 - A_0)} \quad \text{for a bundle of } n \text{ rods (total blockage)}
\]

**Fig. 139** General blockage definition

**Fig. 140** Examples of blockage
A transparent 'mask' representing the original lattice is laid over the specimen.

Final rod positions lie outside original lattice.

Original rod positions.

Only the area inside the 'mask' is measured. This eliminates the possibility of total blockage > 100%.

Fig. 141 'Mask' method for measurement of blockage.

Fig. 142 Deficiencies of the 'total area' method.
\[ A_0 = \text{original unstrained rod cross-sectional area} \]
\[ p = \text{inter-rod spacing (pitch)} \]
\[ A_{sc} = \text{final sub-channel cross-sectional area} \]
\[ B_{sc} = \text{sub-channel blockage (\%)} \]

\[ B_{sc} = \left[ 1 - \frac{A_{sc}}{(p^2 - A_0)} \right] \times 100 \% \]

**FIG 14.3**
MATHEMATICAL DESCRIPTION OF SUB-CHANNEL BLOCKAGE

**FIG 14.4**
MEASUREMENT OF SUB-CHANNEL PERIMETERS AND ROD GAPS

SUB-CHANNEL PERIMETER
- \( P_{sc} = ABCDE \)
- \( G_{sc} = AE \)

AS PERCENTAGES
- \( P_{sc} = \frac{P_{sc}}{d} \times 100\% \) (d = ORIGINAL ROD DIAMETER)
- \( G_{sc} = \frac{G_{sc}}{(p-d)} \times 100\% \) (p = ROD PITCH)
**Fig 145**

Sub-channel boundary

**Fig 146**

Treatment of burst 'flare outs'

---

**Option A** - Corresponds to a maximum blockage

1) Approximate the shape of the burst Rod just prior to its bursting
2) Measure the subsequent area and perimeter

**Option B** - Corresponds to a maximum blockage

1) Join the ends of the burst opening with a straight line
2) Measure the subsequent area and perimeter
Input data are specified

Initial conditions are computed

Fuel and cladding temperatures are computed

Temperature in fuel rod plenum computed

Fuel and cladding deformation is computed

Internal gas pressure is computed

Fission gas release calculated

New timestep

**Fig. 147** Simplified FRAP-T3 flow chart.
TIME = 0
STEADY STATE

SET NEXT TIME STEP

GEOMETRY

THERMAL - HYDRAULICS

FUEL CONDUCTION

INTERNAL PRESSURE

CONVERGED?

END?

NO

CREEP

YES

YES

NO
**Fig. 149** LOG ($\dot{\varepsilon}$) VS LOG ($\sigma$) PLOT FOR THREE $T$ SPECIMENS DEFORMED AT 873 K. FOR TWO OF THE SPECIMENS, THE POINTS ARE NUMBERED TO INDICATE THE ORDER IN WHICH THE STRESS LEVELS WERE APPLIED. (176)

**Fig. 150** THE STRESS DEPENDENCE OF CREEP ACTIVATION ENERGY. (176)

**Fig. 151** LINEAR REGRESSION LINES THROUGH LOG ($\dot{\varepsilon}$) VS $\sigma$ DATA FOR $L$, $T$ AND $ST$ SPECIMENS. (176)
FIG. 152 EFFECT OF VARIATIONS WITHIN THE MANUFACTURING SPECIFICATIONS ON THE PREDICTED STRAIN (183)
Membership of the Task Group at the first meeting on 22nd March 1983:

Canada

Mr. E. Kohn
Engineer
Atomic Energy of Canada Ltd
Section Head, Fuel Behaviour
Engineering Company
Sheridan Park Research Community
Mississauga
Ontario L5K 1B2

Mr. M. Notley
Branch Head, Fuel
Chalk River Nuclear Laboratories
Chalk River
Ontario, K0J 1J0

Finland

Mr. S. Kelppe
Research Engineer
Technical Research Centre of Finland
VTT/YDI
P.O. Box 169
SF-00181 Helsinki 18

France

Mrs. B. Houdaille
Ingenieur
Services des elements combustibles et struct.
CEN Saclay
91191 Gif sur Yvette
CEDEX

Dr. P. Berna
Section d'Etudes des Accidents des Reacteur à Eau Légère
IPSN CEN Cadarache B1245
BN01, B115 St Paul Cy Durance

Germany

Mr. A. Fiege
Kernforschungszentrum Karlsruhe GmbH
Projekt Nukleare Sicherheit
Postfach 3640, D-7500 Karlsruhe

Dr. F. Wunderlich
Physicist
c/o KWU, Postfach 3220
D-8520 Erlangen

Prof. H. Karwat
Technische Universität München
c/o Gesellschaft für Reaktorsicherheit mbH
Forschungsgelände
D-8046 Garching
Switzerland

Mr. U. Schmocker
Physicist
Swiss Nuclear Safety Department
HSK c/o EIR
CH-5303 Würenlingen

Mr. H.P. Grutter
Engineer
EIR
CH-5303 Würenlingen

United Kingdom

Mr. C.A. Mann
Group Leader
Water Reactor Fuel Element Evaluation
Springfields Nuclear Laboratories
UKAEA
Salwick
Preston, PR4 ORR

Mr. D.O. Pickman
Head of Laboratories
Springfields Nuclear Laboratories
UKAEA
Salwick
Preston, PR4 ORR