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Assessment of VHTR core design with regard to fuel temperature
Loss Of Forced Cooling conditions

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Assessment of VHTR core design with regard to fuel temperature
Loss Of Forced Cooling conditions

Abstract

The very high temperature reactor (VHTR) is one of the six reactor types selected in the Generation-IV research program. The VHTR is a graphite-moderated, helium-cooled reactor with a once-through uranium fuel cycle. It is designed to be highly efficient, to be flexible to adopt to the uranium/plutonium fuel cycle, to have a minimal waste production, while retaining its inherently safe characteristics.

The results presented in this report relate to the preliminary investigations carried out on the VHTR design. The objective of these studies was to understand the physical aspects of the annular core and to identify the limits of both standard block type and pebble-bed VHTR designs with regard to a degradation of its passive safety features due to an increase of the operating temperature. From those results, possible improvements of the performances in term of power density and outlet temperature have been analyzed.

Results for nominal operating conditions can be found in "Assessment of VHTR core design with regard to fuel temperature; Normal operation", (D-SI2.1). This report (D-SI2.2) emphasizes on the fuel temperature in the core during transients, i.e. Loss Of Forced Cooling conditions.

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RAPHAEL System Integration
Optimization of Very High Temperature Reactor core
designs: Loss Of Flow Conditions

October 31, 2008
Summary

The very high temperature reactor (VHTR) is one of the six reactor types selected in the Generation-IV research program. The VHTR is a graphite-moderated, helium-cooled reactor with a once-through uranium fuel cycle. It is designed to be highly efficient, to be flexible to adopt to the uranium/plutonium fuel cycle, to have a minimal waste production, while retaining its inherently safe characteristics.

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Pebble-Bed VHTR

An analysis of the 400 MW(th) Pebble Bed Modular Reactor (PBMR) design shows that the inherent safety concept that has been demonstrated in the smaller AVR and HTR-10 in practice, can also be applied to a commercial size reactor. The fuel temperatures of this design are below the limits, both during nominal operation as well as during anticipated loss-of-coolant-flow transients. However, it is shown that the temperature during a Depressurized Loss Of Forced Cooling (DLOFC) incident, for a small part of the core will reach the 1600 °C limit after 22 hours, without active intervention. Therefore, a further increase of the reactor power to raise the helium outlet temperature is unattractive.

The total number of times that a certain pebble is (re)introduced in the core can be increased to flatten the axial power and the fuel temperature profile. The effect has been analyzed by linking the DALTON-THERMIX code system with fuel depletion analysis calculations using SCALE. For nominal operation a total of six pebble passes is optimal
since the peak in the axial power profile in the top region of the core matches the cool helium temperatures in this region. For a DLOFC case in which the maximum fuel temperature is determined largely by the peak in the power profile a further increase to ten pebble passes is found to be advantageous. By creating several radial fuel zones in the core in combination with multiple pebble recycling steps the radial distribution of fissile material can be influenced. For a two zone core it was found that by recycling the pebbles eight times in the outer zone and two times in the inner zone consecutively, the peak in the radial power profile reduces from 10 MW/m$^3$ to 8.7 MW/m$^3$. The maximum power density can be further reduced to 8.2 MW/m$^3$ using three radial fuel zones. For an improved pebble bed design with three radial fuel zones, the maximum fuel temperature during normal operation and during a DLOFC transient is reduced by 80 °C and 300 °C, respectively.

By altering the coolant flow from axial to radial direction, the pressure drop in the pebble bed can be reduced tremendously, which results in an increase of the plant efficiency with several percent. The resulting increase in fuel temperature can be compensated by reducing the pebble size or by recycling the pebbles from the outside of the core to the inside. In the latter case, the power profile has its peak on the outside of the core, which is also advantageous in a DLOFC case. A Once-Through-Then-Out (OTTO) fueling scheme can be used to approximate the optimal power profile yielding an axially flat fuel temperature profile. The resulting high peak in the power profile which causes high temperatures in a small region of the core during a DLOFC transient are not expected to lead to a high particle failure probability, since the particles in that region have a low fuel burnup level.

An increase of the reactor power is allowable if one or more of the above proposed design or fuel management modifications would be implemented in current reactor designs. This would lead to an increase of the reactor performance and the availability of high temperature helium for process heat applications.

**Prismatic VHTR**

**Reactor performance during transient scenario (loss of coolant accident)**

The objective of the analysis was to evaluate the influence of some design parameters on the passive safety features of the VHTR 600 MWth annular core configuration. Three major modifications were analyzed. It concerns the average power density, the core height and the coolant flow through the core. Moreover, we also investigated the impact of the reflector material. The results showed that the passive safety features of the annular core configuration are strongly affected by the increase of the average power density. The increase of the active core height is less damaging for both nominal operation and transient scenario. After all, the replacement of the inner reflector and part of the external reflector by material with better thermal properties (MgO) allows to gain 40 °C on
the maximum fuel temperature.

**Decay Heat Removal in a prismatic VHTR including heat transfers through the reactor pit**

A high temperature reactor (HTR) is based upon a massive graphite core and a flow of helium which acts as coolant. One interesting safety-related feature of an HTR is its passive heat removal capability in case of loss of a cold sink. Previous studies done at Areva-NP and EDF on a prismatic block concept have investigated depressurized conduction cooldown as well as pressurized cases. Areva-NP had done its investigations on a limited angular sector (sector of 30°) while EDF had simulated half a HTR including the hot duct. However, in both studies, boundary conditions were prescribed directly on the external side of the vessel using correlations. The purpose of this paper is to present exploratory studies including thermal heat transfers taking place through the reactor pit, using the CFD code Code-Saturne coupled with the thermal code SYRTHES. Code-Saturne is a general purpose CFD finite volume code which handles the flow convective aspects, while SYRTHES is a taking care of conduction inside solid parts and wall to wall radiation using respectively a finite element method and a radiosity approach.

A flexible numerical technique has been used. It is based upon the coupling at each time step during the thermal transient between one instance of the thermal code SYRTHES and two different instances of the CFD code Code-Saturne. The first one is in charge of the helium flow inside the vessel while the second takes into account the air natural convection flow which develops inside the reactor pit. Similarly to previous studies, all prismatic graphite blocs are represented but an homogeneous approach is retained to model the fuel blocs forming the annular core in which the helium flows. The influence of several parameters on the temperature reached in different parts is investigated. Calculations presented are not design calculations but remains general exploratory computations and explain the numerical methodology retained at EDF to handle some decay heat removal aspects. Likewise, the paper underlines that such a numerical approach could be applied to other type of reactors like Sodium Fast Reactor (SFR) or Gas Fast Reactor (GFR) where the decay heat removal may also turn out to be a challenging task.
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Chapter 1

Introduction

The German fuel pebble design, as applied to the PBMR, and American block type design, as selected in the ANTARES programme, are consistent with normal operation core outlet temperatures of 900 °C and 850 °C respectively. Such designs should be applicable as long as operating conditions are limited to these values. For the case of a Very High Temperature Reactor to be used to produce heat for a hydrogen production process, more demanding operating conditions and more especially a higher core outlet temperature would be desirable, about 900 to 1000 °C. Questions regarding the applicability of the existing core and fuel designs are then raised.

1.1 Objectives

The objective of this report is to investigate the limits of the existing designs and to propose, if necessary, upgraded arrangements to cope with the higher gas temperatures.

- As a first step, the generic issues of the fuel temperature during normal operation, and more precisely the fraction of fuel in the core above a given value is investigated. It is commonly agreed that this parameter determines the extent of fission product release by diffusion through the fuel coating layers and the extent of primary coolant contamination under normal operation.

- Design solutions will be investigated to reduce fuel temperature, by minimizing the difference between helium coolant and fuel temperatures, or by flattening the power distribution over the core especially in the vicinity of the graphite reflectors. The results for nominal operating conditions can be found in "Assessment of VHTR core design with regard to fuel temperature; Normal operation", (D-SI2.1).

- This report (D-SI2.2) emphasizes on the fuel temperature in the core during transients, i.e. Loss Of Forced Cooling conditions. The transient investigated will be the depressurized cooldown transient, recognised as an envelope situation for the peak fuel temperature. The work aims at determining the peak fuel temperature
and its related distribution over the core under VHTR operating conditions and to investigate possible solutions for generating margins.
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RAPHAEL System Integration  
Optimization of Very High Temperature Reactor core designs:  
Loss Of Forced Cooling conditions

B. Boer, J.L. Kloosterman  
PNR-131-2008-011  
October 2008

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Chapter 2

Reactor performance of the Pebble-Bed VHTR during transients (TUD)
Summary

This part of the report deals with the pebble-bed type VHTR that aims at an increased coolant outlet temperature of 1000 °C and beyond to increase reactor performance and to allow for cost-effective production of hydrogen using nuclear energy.

An analysis of the 400 MWth Pebble Bed Modular Reactor (PBMR) design shows that the inherent safety concept that has been demonstrated in the smaller AVR and HTR-10 in practice, can also be applied to a commercial size reactor. The fuel temperatures of this design are below the limits, both during nominal operation as well as during anticipated loss-of-coolant-flow transients. However, it is shown that the temperature during a Depressurized Loss Of Forced Cooling (DLOFC) incident, for a small part of the core will reach the 1600 °C limit after 22 hours, without active intervention. Therefore, a further increase of the reactor power to raise the helium outlet temperature is unattractive.

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An increase of the reactor power is allowable if one or more of the above proposed design or fuel management modifications would be implemented in current reactor designs. This would lead to an increase of the reactor performance and the availability of high temperature helium for process heat applications.

### 2.1 Introduction

#### 2.1.1 The reference High Temperature Reactor design: The Pebble Bed Modular Reactor

The Pebble Bed Modular Reactor (PBMR) is being developed by ESKOM, the electric utility of South Africa. The PBMR is a pebble-type High Temperature Gas Cooled Reactor (HTGR). The PBMR power plant incorporates a closed cycle primary coolant system utilizing helium to transport heat energy directly from the modular pebble bed reactor to a recuperative Brayton cycle power conversion unit with a single-shaft turbine/compressor/generator. In Tab. 2.1 the general design characteristics of the PBMR are presented [2-1].

<table>
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<th>Characteristic</th>
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<td>Reactor power</td>
<td>400 MW</td>
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<tr>
<td>Energy conversion system</td>
<td>Brayton cycle</td>
</tr>
<tr>
<td>Plant electrical power</td>
<td>165 MW</td>
</tr>
<tr>
<td>Coolant</td>
<td>helium</td>
</tr>
<tr>
<td>Inlet temperature</td>
<td>500 °C</td>
</tr>
<tr>
<td>Outlet temperature</td>
<td>900 °C</td>
</tr>
<tr>
<td>Moderator</td>
<td>Graphite</td>
</tr>
<tr>
<td>Fuel type</td>
<td>Pebbles</td>
</tr>
<tr>
<td>Pebble core passes</td>
<td>6</td>
</tr>
<tr>
<td>Fuel loading</td>
<td>15,000 TRISO’s (9 g) per pebble</td>
</tr>
<tr>
<td>Fuel isotopic composition</td>
<td>UO$_2$ with 9.6 % enr. U-235</td>
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<tr>
<td>Core geometry</td>
<td>Annular with fixed centre reflector</td>
</tr>
<tr>
<td>Core height</td>
<td>11 m</td>
</tr>
<tr>
<td>Core width</td>
<td>1 m inner and 1.85 m outer radius</td>
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The PBMR is the reference design in the calculations and optimization of the following sections.

#### 2.1.2 Outline

In the following sections the design of the PBMR is optimized. First, nominal operation and transient analysis of the reference design is performed (Sec. 2.2). Secondly, the effect
of altering the pebble recycling scheme to improve the performance is analyzed and the design is optimized in Sec. 2.3. In Sec. 2.4 the direction of the coolant flow is altered to further increase the performance of the reactor. The last section (Sec. 2.5) deals with a reactor core that adopts the Once-Through-Then-Out (OTTO) scheme for the pebble fuel management.

2.2 Simulation of the Pebble Bed Modular Reactor

The Pebble Bed Modular Reactor (PBMR) is a High Temperature gas cooled Reactor that is currently being designed by the PBMR Ltd. Steady state and transient benchmark exercises were organized by the Nuclear Energy Agency (NEA) [2-1]. In this section the results of the DALTON-THERMIX code system for a coupled steady state and (De)Pressurized Loss Of Forced Cooling (DLOFC/PLOFC) transient cases of the PBMR are verified with the results of the TINTE code system [2-2] made available by the PBMR Ltd. [2-3].

In this section the effect on the operating temperature for increased reactor power and size is investigated, going form a demonstration size, such as the HTR-10 (10 MW) and AVR (46 MW) reactors, to a commercial size of 400 MW.

![Neutron flux profiles](image)

Figure 2.1: The fast 2.1(a) and thermal 2.1(b) flux profiles in the PBMR at normal operating conditions showing the location of the peaks. The solid line shows the region of the pebble bed.

The control rods that are located in the side reflector, are modeled as a uniform 'grey curtain'. The plenum above the pebble bed and the helium gap between the side reflector
and the core barrel are modeled as void regions using directional diffusion coefficients [2-4].

Neutron cross sections that depend on the fuel temperature, moderator temperature, xenon concentration, local fast and thermal buckling were provided as part of the benchmark description [2-1]. The buckling dependence was included to capture the spectral effects resulting from changes in the environment of a given core region. The local buckling of group \( g \) in a certain mesh volume is defined as:

\[
B_g^2 = -\nabla \cdot \frac{D_g \nabla \phi_g}{D_g \phi_g}.
\]

(2.1)

**Normal operating conditions**

A coupled neutronics / thermal-hydraulics calculation is performed to determine the conditions of the reactor during normal operation. In this case the resulting neutron flux profile from a DALTON eigenvalue calculation was normalized to a fixed power level of 400 MW, while the \( k_{eff} \) was allowed to deviate from exact criticality. Several iterative runs of DALTON and THERMIX are performed until convergence is reached on the \( k_{eff} \). The results for the neutron flux profiles, axial power and temperature profiles are shown in Figs. 2.1(a), 2.1(b), 2.2 and 2.3.

The flux profiles in Figs. 2.1(a) and 2.1(b) show a peak at the top of the pebble bed, which is caused by the fact that fresh fuel is inserted at the top of the core and removed at the bottom. The thermal flux profile shows peaks in the inner and outer reflector, resulting in power peaks near the radial edges of the pebble bed.

In Table 2.2 some key results of the two code systems are compared. The power profile in DALTON has a higher peak, which also results in a higher maximum neutron flux compared to TINTE. The differences for the average temperatures are within several degrees C. This can also be seen from Fig. 2.2 in which the axial profiles of the maximum and average fuel temperatures are shown. The small differences can be attributed to the difference in the power profile (see Fig. 2.3), resulting in higher temperatures for the DALTON-THERMIX results in the region of the power peak.

In general, a good agreement between the two code systems is found for the steady state results, although some differences in the neutronic results were encountered. In order to start from an exactly critical reactor at the beginning of the transient calculations the fission source was scaled with the eigenvalue.

**Depressurized Loss Of Forced Cooling**

The first transient is a Depressurized Loss Of Forced Cooling (DLOFC) without SCRAM. Starting from full load operating conditions, the mass flow is reduced to 0.2 kg/s and the pressure to 1 bar assuming a linear reduction over 13 seconds. In a second case, assuming a SCRAM after 13 seconds, the control rods are fully inserted within 3 seconds. Furthermore, the mass flow is reduced to 0 kg/s instead of the trickle flow of 0.2 kg/s.
Table 2.2: Comparison of Steady State results between DALTON-THERMIX and TINTE

<table>
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<th>TINTE</th>
<th>Unit</th>
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<tr>
<td>$k_{eff}$</td>
<td>1.0016</td>
<td>0.9940</td>
<td>-</td>
</tr>
<tr>
<td>Maximum Power density</td>
<td>10.84</td>
<td>10.55</td>
<td>W/m$^3$</td>
</tr>
<tr>
<td>Maximum fast flux</td>
<td>$2.27 \times 10^{14}$</td>
<td>$2.14 \times 10^{14}$</td>
<td>cm$^{-2}$s$^{-1}$</td>
</tr>
<tr>
<td>Maximum thermal flux</td>
<td>$3.22 \times 10^{14}$</td>
<td>$3.17 \times 10^{14}$</td>
<td>cm$^{-2}$s$^{-1}$</td>
</tr>
<tr>
<td>Outlet helium temperature</td>
<td>898.3</td>
<td>898.5</td>
<td>°C</td>
</tr>
<tr>
<td>Pressure drop</td>
<td>297.7</td>
<td>294.2</td>
<td>kPa</td>
</tr>
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<td>Average fuel temperature</td>
<td>808.9</td>
<td>813.0</td>
<td>°C</td>
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<tr>
<td>Average moderator temperature</td>
<td>795.5</td>
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<tr>
<td>Average helium temperature</td>
<td>750.7</td>
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Figure 2.2: Comparison of the axial profile of the average and maximum fuel temperatures for DALTON-THERMIX and TINTE results at normal operating conditions of the PBMR.

Figure 2.3: Comparison of the axial power profile of DALTON-THERMIX and TINTE results at normal operating conditions of the PBMR.

The response of the reactor power for both cases (with or with SCRAM) of the DALTON-THERMIX calculation during the first 300 seconds is presented in Fig. 2.4. The temperature feedback causes a reduction in the fission power of the reactor. In the case that a reactor SCRAM is included, the fission power is reduced rapidly and the total reactor power is determined by the decay heat with in seconds after the SCRAM.

In the case that no SCRAM has been performed, the reactor becomes critical again after it has sufficiently cooled down. The point of re-criticality occurs in the DALTON-THERMIX calculation after 42.7 h, compared to 43.5 h for the TINTE calculation. After several oscillations in power and temperature the fission power reaches a quasi-steady state (see Fig. 2.5), while both the temperature of the pebble-bed and of the reflector increase in the following hours (see Fig. 2.6).
Although the results of the temperatures and the point of re-criticality are similar for both DALTON-THERMIX and TINTE, significant differences can be identified. DALTON-THERMIX predicts a slightly higher value for the maximum fuel temperature, which can be explained by the different implementation of the decay heat distribution in the reactor model of TINTE. In DALTON-THERMIX it was assumed, according to the benchmark specification, that the shape of the decay heat profile is the same as the power profile at steady state. The TINTE decay heat model is based on the DIN25485 standard [2-5]. The initial decay heat (at the start of the transient) is calculated using operational history data passed from the VSOP code [2-6], including power levels and fuel composition data (fractional fission rate per isotope). During the transient analysis, the detailed DIN standard is used to obtain a time-dependent expression for the decay heat. This calculation is based on the fission rate for each node at each time interval, the shape of which changes during the transient. As a consequence the axial power profiles used in the two codes, shown in Fig. 2.7, are different at the end of the transient. The power profile used in TINTE is clearly more flattened than the power profile used in DALTON-THERMIX. Furthermore, the total reactor power at the end of the transient is higher for the DALTON-THERMIX calculation than for the TINTE calculation, respectively 2.63 MW and 2.24 MW. This power corresponds to the heat that is dissipated through the side of the RPV. This explains the difference in the maximum and average fuel temperature after re-criticality 2.8. The fuel temperature histories for the

![Figure 2.4: Power (total and decay) history of DALTON-THERMIX for the first 300 seconds of the DLOFC transient showing the effect of a SCRAM.](image1)

![Figure 2.5: Power (total and decay) history of DALTON-THERMIX results (between 40 and 100 hours) of the DLOFC (no SCRAM) transient.](image2)

DLOFC with SCRAM are presented in Fig. 2.8. The resulting temperatures are higher than for the case without the SCRAM since the trickle flow of 0.2 kg/s is absent. The results for the average fuel temperature are similar while the maximum fuel temperature is higher in the DALTON-THERMIX result. This is again caused by the different treatment of the decay heat distribution (see Fig. 2.7). Because the DLOFC case with
SCRAM does not contain fission power, the power profiles of TINTE for both cases are different, since the decay heat is distributed differently from the prompt power. In DALTON-THERMIX the power profile of the fission power and decay power is similar and therefore the resulting profiles for the two cases are almost identical.

![Figure 2.6: Maximum (max) and Average (ave) temperature history of DALTON-THERMIX and TINTE results for the DLOFC transient (without SCRAM)](image)

![Figure 2.7: Axial power profiles calculated with DALTON-THERMIX (D-T) and TINTE (T) at the end of a DLOFC transient with SCRAM and without SCRAM, respectively after 50 and 100 hours. The power profile is largely determined by the decay heat profile, which has a fixed shape in the DALTON-THERMIX calculation, while for the TINTE result the shape of the decay heat profile has been updated throughout the transient.](image)

**Pressurized Loss Of Forced Cooling with SCRAM**

The Pressurized Loss Of Forced Cooling (PLOFC) case is similar to the DLOFC case with SCRAM with the difference that the system pressure is reduced from 90 bar to 60 bar during the first 13 seconds of the transient and the mass flow is reduced to 0 kg/s instead of 0.2 kg/s. After the pressure reduction it is assumed that the helium inventory in the reactor remains constant, allowing the pressure to vary over time depending on the helium temperature.

The power history of the PLOFC transient is almost identical to the power history for the DLOFC with SCRAM of Fig. 2.4 except for minor differences during the first 13 seconds.

The results for the fuel temperatures in Fig. 2.9 of the two codes again show a similar trend with DALTON-THERMIX predicting a higher maximum fuel temperature. The
The difference in the maximum fuel temperature for the two codes is again sought in the different interpretation of the decay heat distribution between DALTON-THERMIX and TINTE.

The natural convection in the reactor increases the ability of the reactor to remove the decay heat, resulting in lower temperatures. In Fig. 2.10 and 2.11 the temperature profiles in the reactor are shown after 50 hours in the PLOFC transient and after 100 hours in the DLOFC transient. It can be seen that for the PLOFC case, which includes natural convection, the heat is transported to the top region of the core. The maximum fuel temperature for the PLOFC is 1382 °C, which is significantly lower than the 1618 °C that was found for the DLOFC case.

2.2.1 Conclusion

In this section a new code system for the evaluation of static and dynamic behavior of pebble-bed reactors has been presented. Comparisons of the results of this code system with experimental data and TINTE of normal operation and LOFC transients were made for the PBMR reactor.

Regarding the modeling of pebble-bed HTRs the following is concluded:

- During normal operation of a pebble-bed reactor the convective heat transfer from the pebbles to the helium coolant effectively removes the heat from the core. Only a small portion (less than 1 % of the total power) of the generated heat is transferred to the surroundings by conduction and thermal radiation. In the absence of forced cooling during D\PLOFC transients, the value of this small heat loss determines the behavior of the reactor. The maximum temperature reached, the rate at which the core cools down thereafter and the consequent time point of re-criticality (if
no SCRAM is performed) are largely dependent of this heat loss. Furthermore, the steady state power level reached after re-criticality (no SCRAM) is equal to the total heat loss of reactor.

- Small core flows, such as bypass flows for cooling of reflectors and control rods can be of major importance to the nominal and transient core behavior. This is caused by the resulting effect on the fuel and moderator temperatures, which have a large temperature reactivity feedback. It is therefore recommended that these flows are recorded during experiments and are incorporated in benchmark exercises.

Conclusion regarding the DALTON-THERMIX code system: The DALTON-THERMIX code system can effectively predict the temperatures in the core during normal and P\DLOFC transients for small and large pebble-bed reactor designs. It captures the dynamic behavior of the reactor and is therefore a useful tool in determining the inherent safety capabilities of pebble-bed HTR designs. Therefore, the DALTON-THERMIX code system forms the basis of the investigations that are presented in the following sections on several pebble-bed VHTR designs.

In the DLOFC case (without SCRAM) of the PBMR it was found that the maximum fuel temperature during the transient is 1648 °C. An increase of the reactor power in the PBMR during normal operation aiming at an increased helium outlet temperature leads to an increased power density and resulting higher fuel temperature.
2.3 Optimization of the pebble recycling scheme

From the previous section it was found that a reduction of the power peak in the core is attractive to decrease the maximum fuel temperature and to increase fuel performance. In this section presents the impact of a several pebble (re)loading schemes in an attempt to reduce peak temperature in the present PBMR design.

2.3.1 Fuel management in pebble-bed reactors

Current pebble-bed reactor designs, such as the HTR-PM and the PBMR, include an inner and outer graphite reflector. The employment of an inner reflector avoids a power peak at the center of the pebble-bed and adds additional thermal capacity to the core, which is beneficial during accident situations. The power profile in the annular pebble bed can exhibit peaks near these reflectors caused by the local abundance of thermal neutrons. Furthermore, the tall core geometry, adopted for thermal-hydraulic reasons, causes a large difference in the burnup level between top and bottom of the core, resulting in an axial power peak at the top.

By using several pebble inlet positions at the top of the core the pebble distribution and therefore the nuclide distribution over the core can be influenced. The power profile can be influenced in the radial direction by systematically placing pebbles with different enrichment or fertile content at a different radial starting position. In the extreme case, a central reflector can be formed using pebbles without fuel for the inner core zone [2-7]. Experiments were conducted in the past using several pebble types and inlet positions during the operation of the AVR reactor [2-8].

The axial power profile can be modified by recycling the pebbles several times through the core. This multiple recycling scheme has a secondary advantage, since it provides the reactor operator with pebbles containing different amounts of fissile nuclides. By recycling these pebbles at different radial reloading positions, for example by placing fresh pebbles in the outer region of the core, it is possible to modify the radial power profile while omitting the use of pebbles with different enrichments or burnable poison [2-9].

Optimizing the pebble loading pattern, thereby improving the power profile, is expected to reduce the fuel temperature for a fixed helium outlet temperature. In this section a calculation tool is used to evaluate several pebble loading schemes. The effect of the improved power profile on the fuel temperature is quantified. The methodology for the calculation of the equilibrium nuclide concentration in the core was presented in the System Integration Report for nominal conditions. The effect of pebble recycling on the axial power profile of this reactor is described in Sec. 2.3.2, followed by an investigation of the effect of radial fuel zoning on the radial power profile (Sec. 2.3.3). Furthermore, an optimization routine is used to find the optimal pebble loading pattern for cores with multiple fuel zones. In Sec. 2.3.5 conclusions are drawn on the possibility of operating present reactor designs are elevated temperatures by using an improved fuel
management scheme.

For the reason of clarity the results for the nominal conditions, which were also given in the report for nominal conditions, have been presented again in this section.

2.3.2 Impact of multiple pebble recycling on the reactor performance

In order to flatten the axial power peak, pebbles are recycled several times through the core before disposal. The effects on the axial power profile and (fuel) temperatures are analyzed for normal operation as well as for a DLOFC accident.

Normal operating conditions

The effect of the total number of pebble passes on the power profile and temperature profiles can be seen in Figs. 2.12 and 2.13. The total number of pebble passes ranges from one to ten. The shape of the profile changes from an exponential shape for one pass toward a more cosine shaped curve for ten passes. This is caused by the fact that the fissile material is spread more evenly over the core height and the burnup difference per pass decreases. The power peak reduces and shifts towards the center of the core with increasing number of pebble passes steps. Consequently, the location of the maximum fuel temperature also moves towards the bottom of the core. Furthermore, the shape of the fuel (pebble center) temperature profile changes into a gradually increasing function from the top to the bottom of the core (Fig. 2.13). From Fig. 2.14 it can be seen that increasing the number of passes beyond six is not advantageous with regard

![Figure 2.12](image1.png)

Figure 2.12: The axial power profile as a function of the axial position with the total number of pebble passes as a parameter. It can be seen that the power profile flattens with increasing number of passes.

![Figure 2.13](image2.png)

Figure 2.13: The maximum fuel temperature as a function of the axial position with the total number of pebble passes as a parameter. The location of the maximum value moves from the top towards the bottom of the core (from left to right in the graph) with increasing pebble passes.

seen that increasing the number of passes beyond six is not advantageous with regard
to the maximum fuel (pebble center) temperature during normal operating conditions. The graph also shows a significant increase in the maximum fuel temperature with increasing reactor power, for a fixed helium inlet temperature and mass flow. The size of the increase is weakly dependent on the number of passes. Depending on the design limit on the maximum allowable fuel temperature, the reactor power can be increased in order to increase the helium outlet temperature, resulting in a helium outlet temperature of 1000 °C at a reactor power of 500 MW.

![Graph showing maximum fuel temperature vs total number of passes for 400 and 500 MW reactor power.](image)

Figure 2.14: Maximum fuel temperature during normal operation as a function of the total number of pebble passes for both 400 and 500 MW reactor power.

**Depressurized Loss Of Coolant conditions**

An DLOFC accident has been analyzed for the reference PBMR design. In this transient, which can be considered as a worst case scenario for an HTR, a pipe break or leak in the primary system causes a fast depressurization of the core. Although the effects of the DLOFC accident are limited by the negative reactivity feedback and the capability of the reactor to transfer the decay heat from the core, fuel temperatures are considerably higher compared to normal operating conditions. The temperature profile in the reactor differs greatly from that during normal operation. The highest temperatures are in this case located in the region of the power peak (Fig. 2.15). Reducing this peak by increasing the number of pebble passes can therefore be effective in reducing this temperature. The effect of pebble recycling on the maximum temperature during the DLOFC transient has been investigated. The power profiles that have been derived in Sec. 2.3.2 for the different recycling schemes are used in THERMIX calculation to perform a DLOFC transient. In this thermal-hydraulic standalone calculation a reactor scram is assumed in the beginning of the transient, which results in a slightly lower temperatures compared the temperature encountered in the reference case. Furthermore, the reactor does not reach re-criticality. The result of the maximum fuel temperature during the transient as a function of the total number of passes is shown in Fig. 2.16 for both a 400 and 500 MW reactor design. Since the power profile is flattened with increasing total number of
Figure 2.15: Fuel and solid structure temperatures in the reference design during a DLOFC transient at the time point of the fuel maximum temperature. The core region is shown with a dashed line. Note that the maximum value is located near the power peak at the inner reflector.

Figure 2.16: Maximum fuel temperature occurring in a DLOFC transient as a function of the total number of passes for two values of the reactor power (400 and 500 MW).

pebble passes, the maximum fuel temperature during the transient is reduced. It can be concluded from Fig. 2.16 that the effect becomes smaller with increasing number of passes, but that the reduction in the fuel temperature from six to ten passes for the transient case is still considerable (100 °C).

Fig. 2.16 shows that increasing the reactor power from 400 to 500 MW with the aim of increasing the helium outlet temperature to 1000 °C results in an increase of the
maximum fuel temperature during a DLOFC transient with roughly 200 °C. The cases with less than five pebble passes are not investigated for a 500 MW reactor since already for the 400 MW design too high temperatures were encountered.

2.3.3 Impact of radial fuel zoning on the reactor performance

It was identified that the radial power profile has peaks near the inner and outer reflector. In an attempt to reduce these peaks, the core is divided into several radial regions. It is assumed that the pebbles follow straight vertical flow lines and that radial mixing is limited as was observed in experiments [2-10]. Therefore no special arrangements have to be made for the guidance of the pebbles except for the placement of the pebbles on the top of the core.

In Sec. 2.3.2 it was found that using a total of ten pebble passes is advantageous with regard to the maximum fuel temperature during a DLOFC case. Therefore, this total number of passes ($N_{tot}$) is adopted throughout the following analysis for the radial zoning.

Fuel zoning effects

To illustrate the effect of radial fuel zoning in pebble bed reactors, a core with two radial (inner and outer) fuel zones is taken as a first example (see Fig. 2.17). It is expected that by placing fresh pebbles in the outer region of the core reduces the radial power peak near the inner reflector. The effect on the power density and $k_{eff}$ is investigated for several recycling schemes. Each pebble first passes $n$ times through the outer fuel region and the remaining number ($N_{tot} - n$) through the inner region. In this way the burnup level in the outer region of the core is lower than in the inner region.

Since each pebble passes the same number of times through the core and has the same velocity for all passes it follows that each pass is represented by the same number of pebbles. The inner and outer radii of the pebble bed are fixed to 1 and 1.85 m, respectively. The size (radius) of the fuel regions is determined by the number of pebble passes in each region. Fig. 2.18 shows the effect of the fuel zoning on the $k_{eff}$ and maximum power density. In the case of a small number of passes in the outer zone, a small zone with a high fissile content is located near the outer reflector. Regions in the pebble bed of high neutron importance are located near the inner reflector and, although to a lesser extend, near the outer reflector. Placing fresh pebbles in these regions results in a high $k_{eff}$, and in a power peak.

By increasing the number of passes in the outer zone, the average fissile concentration of the outer zone reduces. Furthermore, the boundary between inner and outer zone shifts and a larger part of the outer zone moves away from the outer reflector. The peak in the power density near the outer reflector is reduced as well as the $k_{eff}$. If more than seven pebble passes are used in the outer zone, the neutron flux in the outer zone is influenced increasingly by the inner reflector, which results in an increase in the $k_{eff}$.
The maximum power peak is not always located at the same position. For a small number of passes in the outer zone, the peak is located next to the outer reflector. With increasing number of passes in the outer zone a second peak near the inner reflector develops. Increasing the number of passes in the outer zone further, creates even a third peak, which is located at the inner radius of the outer zone. This explains the slight increase in the maximum power density going from three to four and from seven to eight number of passes in the outer zone (Fig. 2.18). In a second approach, the pebbles are recycled first in the inner region and then in the outer region. In general, this results in both a higher $k_{\text{eff}}$ and a higher power peak than for the first approach (Fig. 2.19), since the fresh fuel is located in the region with a high neutron importance. For an increasing number of cycles in the inner region, the size of this region increases while the fissile concentration in this zone decreases. The first mentioned effect results in an increase of the $k_{\text{eff}}$, while the second effect decreases the $k_{\text{eff}}$. Therefore, an optimum exists for the $k_{\text{eff}}$ when the pebbles are recycled in the inner zone three times. From the viewpoint of fuel efficiency, the inside-outside recycling scheme gives a better performance, at the expense of a high power peak near the inner reflector.

From the above it can be concluded that the maximum power density can be as low as 8.7 MW/m$^3$ by adopting a two zone recycling scheme. For this calculation, without temperature feedback, the resulting $k_{\text{eff}}$ of 1.027 is lower than a core with only one zone ($k_{\text{eff}}$ of 1.040) which has a maximum power density of 10.0 MW/m$^3$. 

Figure 2.17: Schematic overview of radial fuel zoning in a pebble bed reactor with an outer fuel zone of low burnup pebbles and an inner fuel zone of low burnup pebbles. The figure shows the cross sectional plane at one of the three pebble discharge tubes.
Radial fuel zoning optimization using a heuristic method

In this section a more general approach for the pebble reloading scheme is presented in which the pebbles are allowed to pass in any order through the different zones. For example, the pebbles can pass the two zone core of Sec. 2.3.3 following the sequence \(\{1,2,1,2,2,2\}\), in which the numbers refer to the different zones.

As in Sec. 2.3.3 the core is divided into \(n\) regions and in each region \(i\) the pebbles can pass \(k_i\) times, while the total number of passes \(N_{\text{tot}}\) is equal to \(\sum_{i=1}^{n} k_i\). As before, the size of the region depends on the number of passes it contains, such that the number of pebbles is conserved.

A heuristic method based on the method of Ref. [2-11] was used to speed up the search for the optimal pebble reloading pattern. The search of the optimal pebble loading pattern starts with a global search in which the power profiles of several randomly chosen loading patterns \(H\) are evaluated. The loading pattern which results in the lowest power peak \(q\) is chosen as the first reference loading pattern \(H_R(1)\).

Three cases are investigated with the optimization routine, namely: no radial zoning (one zone), two radial zones and three radial zones. The average radial power profiles of the three cases are shown Fig. 2.20. It can be seen that the maximum power density reduces with increasing number of zones. For the three zone case, the radial profile is considerably flattened and the maximum power density is reduced to 8.2 MW/m\(^3\) (Fig. 2.21). Fig. 2.20 also shows the optimal power profile which is a flat line with an average power density of 4.78 MW/m\(^3\). It shows that a further reduction of the maximum power density can be expected by increasing the number of zones although the size of the zones will be reduced towards the size of the pebbles.
Figure 2.20: Average radial power profile for one, two and three radial refueling zones. For the three zone core the pebbles are cycled through the three zones following the sequence: \{2, 2, 1, 2, 1, 3, 2, 3, 3, 1\}.

Figure 2.21: Power profile of a two (left) and three (right) zone core, showing the flattening effect of the radial fuel zoning.
Figure 2.22: Pebble center temperatures in cores with two (left) and three (right) radial zones, showing a reduction in the maximum temperature with increasing number of zones for normal operating conditions.
2.3.4 Fuel temperature of the improved design

The influence of the improved power profile on the fuel temperature during normal and a DLOFC transient have been investigated with the DALTON-THERMIX code system. The results are presented in Figs. 2.22 and 2.23, respectively.

Since the power profile is flattened for a core with three radial fuel zones, the temperature profile is also flattened (Fig. 2.22). The maximum fuel temperature is lowered with more than 30 °C and 80 °C as compared to the reference case for the two and three zone cores, respectively.

Because the maximum power density is reduced considerably for the improved design, the peak in the temperature profile that occurs in the DLOFC transient of the reference case is reduced (see Fig. 2.23). The maximum temperature occurring during the transient is reduced with approximately 300 °C.

![Graph showing fuel temperature over time for cores with two and three zones.]

Figure 2.23: Average (ave) and maximum (max) fuel temperatures during a DLOFC transient in cores with two and three zones.

2.3.5 Conclusion

The fuel loading pattern of the PBMR-400 was optimized with regard to the fuel temperature. The following conclusions are drawn:

1. By adopting a multi recycling scheme for the pebbles the axial power profile is flattened and the maximum fuel temperature is reduced with increasing number of pebble cycles. Beyond six pebble passes no significant improvement is found for the maximum fuel temperature for normal operating conditions. However, an additional reduction of the maximum fuel temperature of 100 °C was found during a Depressurized Loss of Forced Cooling accident case if a total of ten pebble passes is used. Using more than ten passes is expected to result in only a small reduction in the temperature for the transient case.

2. By creating several radial fuel zones in the core in combination with multiple pebble recycling steps the radial distribution of fissile material can be influenced. For a
two zone core it was found that by recycling the pebbles eight times in the outer zone and two times in the inner zone consecutively, the peak in the radial power profile reduces from 10 MW/m$^3$ to 8.7 MW/m$^3$.

3. The maximum power density can be further reduced to 8.2 MW/m$^3$ using three radial fuel zones. In this case the pebbles are (re)loaded following a specific sequence found with an optimization routine.

4. In an improved pebble bed design with three radial fuel zones, the maximum fuel temperature during normal operation and during a DLOFC transient is reduced by 80 °C and 300 °C, respectively.

2.4 Optimization of the coolant flow

2.4.1 Introduction

In current High Temperature Reactor (HTR) designs of the pebble bed type, such as the Pebble Bed Modular Reactor (PBMR-400) and the HTR-PM, the helium coolant flows from top to bottom through the core. The pressure drop over the pebble bed is considerable, especially for HTRs that have a large core height, such as the PBMR-400 design. For the PBMR-400 with a core height of 11 m the pressure loss of the bed ($\Delta p \approx 2.8$ bar) results in a circulator power that is more than 7 % of the net power generated.

By altering the primary direction of the coolant flow from the axial to the radial direction, the pressure drop can be reduced tremendously [2-12]. In that case the coolant flows from the outer reflector through the pebble bed and finally to flow paths in the inner reflector, through which the coolant exits the reactor. The cooling flow paths in the reactor for radial and axial cooling are shown in Fig. 2.24. The low pressure drop in the radially cooled reactor allows for a reduction in the pebble size that reduces the fuel temperature.

In this section the effects of pebble size reduction on the pressure drop and the maximum fuel temperature are quantified. First, an analytical expression is derived to calculate the pressure drop and fuel temperature in a radially cooled reactor. In a second step a two-dimensional numerical model is used to calculate the effects.

It is shown that the radial power profile can be improved by recycling the pebbles several times in three separated radial fuel zones [2-13]. This reduces the fuel temperature significantly. A theoretical optimum for the radial profile can be derived analytically and can be approximated by adopting several radial fuel zones.

The combined effect of pebble size reduction and adoption of radial fuel zones, up to ten zones, is investigated in this section for normal operating conditions and Loss Of Coolant Accident (LOCA) conditions using existing codes and methods.
2.4.2 Optimization of pebble size and the power profile by radial fuel zoning for a radially cooled core

Besides reducing the pebble diameter, the power profile can also be optimized in order to reduce the fuel temperatures. A numerical calculation is used to determine the resulting power profile for several pebble loading patterns.

Effect of radial fuel zoning on the power profile and fuel temperature

The optimal power profile is approximated by dividing the core into several radial fuel zones. A multi-pass recycling scheme is adopted in which pebbles with a low burnup are placed in the outer core region and high burnup pebbles in the inner region. In the case that the total number of pebble passes through the core is larger than the number of fuel regions, a fuel zone can contain pebbles having different burnup levels.

Fig. 2.25 shows a pebble recycling scheme for a three zone core in which pebbles pass...
through the core ten times. The first pass of the pebbles is in the outer fuel zone and each consecutive pass is either in the same zone or in a zone closer to the inner reflector. Because fresh fuel is now present on the outside of the core and depleted fuel on the inside, the power level has increased on the outside and decreased on the inside.

After each time a pebble has passed through the core the burnup level is measured to determine its burnup level and consequent reloading position. The pebbles reach the target burnup of 95 MWd/kg after ten passes.

The power profile for the proposed loading pattern is calculated with a numerical procedure that is also used in the analysis of HTR fuel under irradiation [2-14]. It consists of the coupled neutronics and thermal-hydraulics DALTON-THERMIX code system [2-15] which is linked to the depletion analysis and neutron cross section generation routines of the SCALE-5 code system [2-16]. The codes are used consecutively until convergence is reached on the discharge burnup, neutron flux and $k_{eff}$.

The resulting power profiles for three and ten radial zones are presented in Fig. 2.26. In the case that ten radial zones are used, each zone represents one pebble pass, while for three radial zones, the pebbles pass through the inner and outer zone three times and four times in the middle zone (Fig. 2.25). The profile for the ten zone case shows a smooth surface, while the three zone case exhibits two jumps at positions of the fuel region interfaces. It can be seen that the discontinuities vanish in the power profile when ten zones, instead of three, are adopted. However, the profile does not significantly improve

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Figure 2.25: Pebble loading pattern for a core with three radial fuel zones and ten pebble passes, showing how the pebbles are recycled from the outside to the inside.
Figure 2.26: Power profiles [MW/m³] for both three (left) and ten (right) radial fuel zones.

with respect to the difference with the optimal profile.

The effect of the power profile, calculated with THERMIX [2-17], on the 2D pebble center temperature profile can be seen in Figs. 2.27 and 2.28. In the case of three radial fuel zones and a pebble radius of 3 cm, three peaks in the temperature profile can be identified that correspond with the peaks in the power profile. If the number of fuel zones is increased to ten and the pebble radius reduced to 0.5 cm only one peak in the temperature profile remains, which is located next to the inner reflector. The difference in the temperature between the outside and inside of the core is larger for the smaller pebble size, since the deviation from the optimal profile is larger for this pebble size.

### 2.4.3 Optimized radial core design

The combined effect of pebble size reduction and the fuel management scheme on the pressure loss and maximum fuel temperature has been investigated with a two-dimensional reactor model in the thermal-hydraulics code THERMIX [2-17] for normal operating conditions.

Three different power profiles are used in the analysis. The first case represents a core configuration without radial fuel zoning (1 zone), while in the two other cases, the power profiles calculated in Sec.2.4.2 for 3 and 10 radial zones are used. In all cases a total of ten pebble passes is used and the pebble radius is varied between 0.5 and 3.0 cm.

The maximum fuel temperature as a function of the pebble radius for several pebble
Figure 2.27: Fuel temperature profiles [°C] in a core with three (left) and ten (right) radial fuel zones for a pebble radius of 3.0 cm.

radii is presented in Fig. 2.29. Both the use of more fuel zones and a reduction of the pebble size result in considerably lower fuel temperatures.

The average pressure drop over the pebble bed calculated with THERMIX for a 3.0 cm pebble radius was found to be 2.1·10⁻³ bar, which compares well with the 2.0·10⁻³ bar that resulted from the analytical calculation. The THERMIX calculation shows that less than 2 % of the total pressure drop occurs in the pebble bed for this case, while the main part is caused by the pressure drop at the slits and flow paths in the inner reflector. The total pressure drop in the core as a function of the pebble radius is shown in Fig. 2.30. Although the pressure drop increases with a reduction of the pebble size it remains more than an order of magnitude smaller compared to the axially cooled pebble bed for all cases.

LOFC conditions

The maximum and average fuel temperatures are calculated for both a Pressurized and Depressurized Loss of Forced Cooling (DLOFC and PLOFC) accident for a core with ten fuel zones and \( R_{\text{peb}} = 0.5 \) cm.

It is assumed that a reactor SCRAM is performed at the beginning of the transients and that the outer surface of the pressure vessel is cooled effectively by the decay heat removal system, which is simulated by a fixed temperature and heat transfer coefficient. Adiabatic boundary conditions are assumed for the top and bottom of the model. During the first 13 seconds of the transients the system pressure is reduced from 90 to 1 bar
Figure 2.28: Fuel temperature profiles [°C] in a core with three (left) and ten (right) radial fuel zones for a pebble radius of 0.5 cm.

in the depressurized case and to 60 bar in the pressurized case. In both cases the mass flow is reduced to zero in the same time period.

In Fig. 2.31 the results for maximum and average temperature during the DLOFC and PLOFC transients are shown. The initial temperature profile for the transients can be seen in Fig. 2.28 and the initial maximum and average temperatures in Fig. 2.31 are 939 °C and 753 °C respectively. In the first few hundred seconds of the transients the maximum fuel temperature reduces quickly since the fission power reduces quickly and the total reactor power consists only of the decay heat. In the following hours, the maximum and average temperature of the pebble bed rise for both the DLOFC and PLOFC cases. The natural circulation in the PLOFC case distributes the heat over the entire core and to the inner reflector. This heat transfer mechanism is absent in the DLOFC case and since the heat is mostly generated in the outer region of the core, the highest temperature is located in this region, while for the PLOFC case the highest temperature occurs at the inner region of the core. The heat of the reactor is removed through conduction in the outer reflector and finally through convection and radiation on the outside of the reactor pressure vessel. Therefore, the heat is more effectively removed in the DLOFC case. The temperature profiles for both cases at the time point of the maximum fuel temperature is presented in Fig. 2.32 to illustrate the above described effects.

The maximum fuel temperature for the two cases is significantly below 1600 °C, which is generally taken as the design limit. Since the power during the transients
Figure 2.29: Effect of pebble size on the maximum fuel temperature for a radially cooled reactor calculated with THERMIX for a core with 1, 3 or 10 radial fuel zones.

Figure 2.30: Effect of pebble size on the pressure drop in the core (pebble bed and reflector flow paths) calculated with THERMIX for a core with 1, 3 or 10 radial fuel zones.

is determined by the decay heat, which is small compared to the fission power during normal operation, the difference in temperature between surface and center of the pebble is small. Therefore the size of the pebbles is of less importance in the determination of the maximum fuel temperature during the transients.

Conclusion

By altering the coolant flow from axial to radial direction, the pressure drop in the pebble bed can be reduced from 2.8 bar to 0.002 bar for a standard pebble size \( R_{\text{peb}} = 3.0 \text{ cm} \). The core average pebble center temperature increases with 100 °C. By reducing the pebble size and altering the (re)fueling scheme of the reactor this temperature can be reduced to 50 °C below the reference temperature, while maintaining the low pressure drop. This would result in a reduction of the pumping power with several percent of the generator power.

By recycling the pebbles from the outside of the core to the inside the analytically
derived optimal temperature profile can be approximated by adopting three or more radial fuel zones. For a standard pebble the improved power profile results in a decrease of the maximum fuel temperature with 125 °C.

The optimized power profile is also advantageous during LOCA accidents since the peak is located at the outer zone of the pebble bed and the decay heat is removed more easily to the environment. In both PLOFC and DLOFC transient cases, the maximum fuel temperature remains below the design limit of 1600 °C and would allow for an increase in reactor power (helium outlet temperature). In the PLOFC transient the natural circulation of the helium between pebble bed and flow paths in the inner reflector causes higher temperatures in the pebble bed after several hours compared to the DLOFC transient. Further investigation on the influence of the geometry of the flow paths and slits in the inner reflector on these natural circulation flows is recommended.

In contrast with the large pressure drop over the pebble bed in an axially cooled design, the total pressure drop in a radially cooled design is determined by losses in the coolant flow paths in the inner and outer reflector.

2.5 The OTTO core

It is possible to derive an analytical expression for the axial power profile that will result in a flat axial fuel temperature distribution [2-18]. The maximum temperature at the pebble center, for a pebble located at an axial position $z$ in the core can be calculated from the helium temperature $T$ and the power density using the following equation [2-18]:

$$T_{\text{max}}(z) = T(z) + \frac{1}{\lambda_{\text{tot}}} q''(z).$$  \hspace{1cm} (2.2)
In this equation \( \frac{1}{\lambda_{tot}} \) is the thermal resistance between pebble center and helium coolant. It is assumed that the helium coolant that flows from the top to bottom of the core is the only heat transfer mechanism between the pebble surface and the final heat sink:

\[
\rho c_p \varepsilon v_z \frac{dT}{dz} = q'''(z)
\]  

Demanding that the maximum temperature described by Eq. (2.2) is constant over the axial direction of the core results in the following equation:

\[
\frac{dT}{dz} + \frac{1}{\lambda_{tot}} \frac{dq'''}{dz} = 0
\]  

By combining Eq. (2.3) and Eq. (2.4) we arrive at a differential equation, which has the following solution for the axial power profile:

\[
q'''(z) = Ae^{-\frac{\lambda_{tot}}{\rho c_p \varepsilon v_z} z}
\]  

This exponential shape can be approximated by using an 'Once Through Then Out' (OTTO) fueling strategy, which results in a high peak in the power profile at the top of the reactor. This peak matches the low helium temperatures in this region of the core, while in the bottom region the high helium temperature is matched by a low power density.

It is noted that the asymmetrical power profile is in general considered to be disadvantageous during loss of flow accidents, in which the power peak results in high...
temperatures at the location of the peak. However, in the OTTO fueling scheme the high temperatures in the top region match the low burnup of the fuel, i.e. low concentration of gaseous fission products in the buffer layer of the TRISO. It will be shown that the stresses and corresponding failure fractions of the coatings remain low, while high fuel temperatures are encountered.

Table 2.3 presents the results for an OTTO core based on the PBMR geometry and two cores with reduced core height and power. For the 400 MW design, the tall core geometry combined with the high target burnup of the fuel, results in a power profile that deviates significantly from the one described by Eq. 2.5. The OTTO scheme therefore does not lead to a flat temperature distribution (see Sec. 2.3.2). Reducing the core height together with a reduction in reactor power, for a fixed average power per pebble, changes the burnup profile (Fig. 2.33) and reduces the maximum power density (Fig. 2.34) which results in a flattened maximum fuel temperature profile (Fig. 2.35).

Table 2.3: OTTO core results.

<table>
<thead>
<tr>
<th>Item</th>
<th>( P = 400\text{ MW} )</th>
<th>( P = 200\text{ MW} )</th>
<th>( P = 100\text{ MW} )</th>
<th>Unit</th>
</tr>
</thead>
<tbody>
<tr>
<td>( q'_{\text{max}} )</td>
<td>33.6</td>
<td>19.7</td>
<td>12.4</td>
<td>MW/m³</td>
</tr>
<tr>
<td>( T_{\text{max}} ) nominal</td>
<td>1436</td>
<td>1192</td>
<td>1100</td>
<td>°C</td>
</tr>
<tr>
<td>( T_{\text{max}} ) DLOFC</td>
<td>2321</td>
<td>1544</td>
<td>1197</td>
<td>°C</td>
</tr>
<tr>
<td>( \sigma_{t,\text{SiC}} ) nominal</td>
<td>-92.4</td>
<td>-85.0</td>
<td>-125.7</td>
<td>MPa</td>
</tr>
<tr>
<td>( \sigma_{t,\text{SiC}} ) DLOFC</td>
<td>-115</td>
<td>-76.2</td>
<td>-122.9</td>
<td>MPa</td>
</tr>
</tbody>
</table>

Figure 2.33: Burnup in an OTTO fuel management scheme as a function of the axial position for three core heights.

Figure 2.34: Power profile in OTTO cores. The power profile is flattened for the cores with reduced heights (H = 550 and H = 275 cm) compared to the OTTO core with reference core height (H = 1100 cm).

A comparison of the tangential stresses in the SiC layer, calculated using the procedure
Figure 2.35: Temperature profile in OTTO cores. The temperature profile is flattened for the cores with reduced heights (H = 550 and H = 275 cm) compared to the OTTO core with reference core height (H = 1100 cm).

Figure 2.36: Tangential stress in the SiC layer as a function of the axial position in OTTO cores with a height of 1100 and 275 cm.

presented in this report (nominal conditions), for both nominal and DLOFC conditions has been made for the three OTTO designs (Fig. 2.36). Note that the stress profile of the core with H = 550 cm has a similar trend as a core with H = 275, but is omitted in this figure for the sake of clarity. For the DLOFC case the temperature profile changes, leading to higher temperatures in the top region and equal or lower temperatures in the bottom region of the core (Fig. 2.35). Since the burnup level and the amount of fission products in the buffer layer is low for the pebbles at the top region, the impact of the increased temperature on the stress is small. On the other hand, the temperatures in the bottom region are reduced in the case of the 1100 cm core height and only slightly increased for the two cases with reduced core heights. For these reasons, the maximum tangential stress during a DLOFC is of the same order of magnitude as the stress during nominal conditions (Table 2.3).

In the stress calculation for the DLOFC conditions it was assumed that there is no significant contribution of increased diffusion of gaseous fission products, caused by the elevated temperatures, since the time period of the transient is short compared to the diffusion process. In reality the resulting increase in internal pressure would cause the SiC tangential stress to be less compressive. Furthermore, the thermal expansion of the coating layers was not taken into account, since the coatings have similar thermal expansion coefficients [2-20] and the resulting stress effect is expected to be small.

It is noted that the maximum temperature in the core for the tall OTTO design exceeds by far the generally taken 1600 °C while the stress in the SiC layer remains in compression, i.e. no SiC layer failure is to be expected. However, the high temperatures that were encountered in the tallest OTTO design are expected to exceed the design
limits of other core components (i.e. core barrel and pressure vessel).

## 2.6 Conclusion

By recycling the pebbles from the outside of the core to the inside for a radially cooled pebble-bed reactor the analytically derived optimal temperature profile can be approximated by adopting three or more radial fuel zones.

The optimized power profile is advantageous during LOFC accidents since the peak is located at the outer zone of the pebble bed and the decay heat is removed more easily to the environment. In both PLOFC and DLOFC transient cases, the maximum fuel temperature remains below the design limit of 1600 °C and would allow for an increase in reactor power (helium outlet temperature). In the PLOFC transient the natural circulation of the helium between pebble bed and flow paths in the inner reflector causes higher temperatures in the pebble bed after several hours compared to the DLOFC transient.

Using an OTTO fueling scheme in the reference core design, in an attempt to approximate the optimal power profile, elevates the fuel temperature, while the tangential SiC stress is comparable to the reference.
2.7 Bibliography

References


Chapter 3

Reactor performance during transient scenario (loss of coolant accident) (CEA)
3.1 Introduction

Two major objectives are presented in this document. In the first part of the analysis, the objective of the calculations is to evaluate some thermal properties of the annular core configuration with respect to various geometrical and physical parameters (bypass fraction, core height and rings number for both internal and external reflector, average core power density, and material properties). In the second part of the analysis, the objective is to evaluate the impact of some design parameters modified in order to increase the core performance during normal operation. In the way to improve the output helium temperature (1000 °C for the average helium output temperature), we tried to evaluate the influence of some modifications on the passive safety features of the annular core configuration. Three major modifications were analyzed. It concerns the average power density, the core height and the coolant flow through the core.

3.2 Description of the modeling

The calculations were performed with CAST3M code [3-1]. An illustration of the core geometry used for the transient calculations is given in Figure 3.1. The thickness of the various structures in the core geometry are listed Table 3.1.

![Figure 3.1: Description of the core geometry used for transient calculation](image)

During the transient, we will focus on the maximum fuel and vessel temperature achieved during the scenario. We also know that the maximum fuel temperature should
Table 3.1: Description of the various material thickness

<table>
<thead>
<tr>
<th>Description of the material</th>
<th>Thickness in m</th>
</tr>
</thead>
<tbody>
<tr>
<td>Internal reflector</td>
<td>1.476</td>
</tr>
<tr>
<td>Fuel zone</td>
<td>0.937</td>
</tr>
<tr>
<td>External reflector</td>
<td>2.306</td>
</tr>
<tr>
<td>Core barrel</td>
<td>0.055</td>
</tr>
<tr>
<td>Helium (steady-state)</td>
<td>0.07</td>
</tr>
<tr>
<td>Void (transient)</td>
<td></td>
</tr>
<tr>
<td>Baffle</td>
<td>0.04</td>
</tr>
<tr>
<td>Helium</td>
<td>0.09</td>
</tr>
<tr>
<td>Vessel</td>
<td>0.19</td>
</tr>
<tr>
<td>Air</td>
<td>2.0</td>
</tr>
<tr>
<td>RCCS</td>
<td>0.05</td>
</tr>
<tr>
<td>Concrete</td>
<td>1.20</td>
</tr>
</tbody>
</table>

not exceed 1600 °C during the transient scenario. The maximum temperature of the vessel must be supervised with the same attention as the maximum fuel temperature. Following the description of the core structure and behind the reactor vessel, there is a space filled with air. The concrete enclosing wall is cooled by water (system RCCS: Reactor Cavity Cooling System) at the temperature of 65 °C. The reactor is dimensioned to be able to evacuate the power without exceeding the maximum temperature of 490 °C for the core vessel and 90 °C for the concrete wall during the nominal operation. These values increase toward 540 °C and 100 °C during accidental situation.

For the calculations, the reactor was modeled explicitly, from the core active zone to the external concrete wall. The calculation is performed, starting from the initial steady state corresponding to the normal operation. The accident corresponds to the total loss of the primary cooling flow (LOFC: Loss Of Forced Convection). All the helium used to cool the core in normal operation left the core vessel. The problem concerns the evacuation of the residual decay heat produced by both heavy nuclides and fission products.

The calculation is performed in two steps. In the first step, we performed the core steady-state calculation in order to determine the initial temperature distribution. In the second step, after the loss of the primary coolant, the transient calculation was performed in order to follow the evolution of the temperature distribution. In the first step, the calculation of the initial temperature distribution is carried out by considering 3D thermal in the solid zones and 1D thermohydraulic in porous media (for both active core and reflectors). In the second step, we considered only thermal conduction in the solid zones. Radiation in the void zones between two interfaces is explicitly treated.
3.3 Results and comments

3.3.1 List of parameters investigated

Various parameters were investigated during the analysis. Starting from the nominal configuration (called reference configuration), we evaluated the influence of various design options such as the average power density, the bypass fraction, the core height, the initial coolant flow in the core, the reflector materials. The parameters in the reference case are the following:

- bypass flow = 28 %
- average power density = 6.55 W.cm\(^{-3}\)
- active core height = 8 m

3.3.2 Nominal configuration

The results issued from the reference calculation are given in Table 3.2. The maximum fuel temperature achieved during the transient is 1558 °C. An illustration of the solid temperature distribution during the transient is given in Figure 3.3 to Figure 3.6.

Table 3.2: Reference case characteristics during transient scenario

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Maximum fuel temperature (°C)</td>
<td>1558</td>
</tr>
<tr>
<td>Time (h)</td>
<td>100</td>
</tr>
<tr>
<td>Maximum core vessel temperature (°C)</td>
<td>486</td>
</tr>
<tr>
<td>Time (h)</td>
<td>137</td>
</tr>
</tbody>
</table>

Figure 3.2: Evolution of the various materials temperature during the transient scenario
Figure 3.3: Temperature distribution in the core during the transient $T = 0\text{h}$ - Steady-state temperature distribution

Figure 3.4: Temperature distribution in the core during the transient $T = 30\text{h}$
Figure 3.5: Temperature distribution in the core during the transient $T = 60h$

Figure 3.6: Temperature distribution in the core during the transient $T = 100h$
3.3.3 Influence of the bypass fraction

During the analysis, we evaluated the influence of the coolant fraction flowing through the active core during the nominal operation. This bypass flow has a strong impact on the nominal temperature distribution and consequently on the evolution of the temperature distribution during the transient. For the analysis, we performed calculation with bypass fraction varying from 0% to 35%. The results of the simulations are reproduced in Figure 3.7 and in Table 3.3. As can be seen in Figure 3.7, taking a bypass fraction equal to 0% (all the helium flows through the active core) has a strong impact on the maximum fuel temperature achieved during nominal operation (significant decrease of the maximum fuel temperature). However, the average reflector temperatures (both inner and outer) are higher and the reflector capability to evacuate the residual decay heat during the transient is strongly reduced. After 10-15h, the maximum fuel temperature

<table>
<thead>
<tr>
<th>Bypass fraction</th>
<th>0%</th>
<th>15%</th>
<th>28%</th>
<th>35%</th>
</tr>
</thead>
<tbody>
<tr>
<td>Maximum fuel temperature (°C)</td>
<td>1591</td>
<td>1557</td>
<td>1558</td>
<td>1560</td>
</tr>
<tr>
<td>Time (h)</td>
<td>96</td>
<td>101</td>
<td>100</td>
<td>100</td>
</tr>
<tr>
<td>Maximum vessel temperature (°C)</td>
<td>500</td>
<td>484</td>
<td>486</td>
<td>487</td>
</tr>
<tr>
<td>Time (h)</td>
<td>122</td>
<td>138</td>
<td>137</td>
<td>136</td>
</tr>
</tbody>
</table>

Figure 3.7: Influence of the bypass fraction on the maximum fuel temperature (T given in °C) during the transient calculation (time given in h)
for 0% case, which was significantly lower than the reference one in the steady-state configuration, becomes higher than the reference case. At the end of the transient, the maximum fuel temperature achieved in the active core is 35 °C higher for the case at 0% than for the case at 28%.

### 3.3.4 Influence of the average power density

In a second step, we evaluated the influence of the average power density in the active core on the maximum fuel temperature reached during the transient. The results are gathered in Table 3.4 and in Figure 3.8. One can notice that the results are strongly influenced by the initial power density. Increasing the power density by 20% induces an increase of the maximum fuel temperature during the transient of 237 °C.

<table>
<thead>
<tr>
<th>Core total power (MW)</th>
<th>600</th>
<th>733</th>
<th>916</th>
</tr>
</thead>
<tbody>
<tr>
<td>Average core power density (MW.m$^{-3}$)</td>
<td>6.55</td>
<td>7</td>
<td>8</td>
</tr>
<tr>
<td>Maximum fuel temperature (°C)</td>
<td>1558</td>
<td>1632</td>
<td>1795</td>
</tr>
<tr>
<td>Time (h)</td>
<td>100</td>
<td>100</td>
<td>100</td>
</tr>
<tr>
<td>Maximum vessel temperature (°C)</td>
<td>486</td>
<td>501</td>
<td>535</td>
</tr>
<tr>
<td>Time (h)</td>
<td>137</td>
<td>137</td>
<td>134</td>
</tr>
</tbody>
</table>

Figure 3.8: Influence of the average power density on the maximum fuel temperature (T given in °C) during the transient calculation (time given in h)
3.3.5 Influence of the core height

In this chapter, the analysis was performed considering various core height. Keeping the same power density, the core height is increased in order to increase the output helium temperature during normal operation. The influence of such increased H/D ratio was also evaluated during the transient scenario. All the results are gathered in Table 5. The increase of the active core height from 8 m to 12 m induces an increase of the maximum fuel temperature from 1558 °C to 1641 °C. However, the increase correlated to the increase of the height is less important than the one observed when increasing the average power density (see in the previous chapter). These results confirm the preliminary trends observed during normal core operation. In order to reach the objective of 1000 °C for the output helium temperature, we demonstrated that the increase of the core height was less penalizing than the increase of the power density.

Table 3.5: Influence of the core height on the maximum fuel and vessel temperatures during the transient calculation

<table>
<thead>
<tr>
<th>Core height (m)</th>
<th>8</th>
<th>10</th>
<th>12</th>
</tr>
</thead>
<tbody>
<tr>
<td>Ratio $\frac{H}{D}$</td>
<td>2.12</td>
<td>2.64</td>
<td>3.17</td>
</tr>
<tr>
<td>Total core power (MW)</td>
<td>600</td>
<td>750</td>
<td>900</td>
</tr>
<tr>
<td>Power density (MW.m$^{-3}$)</td>
<td>6.55</td>
<td>6.55</td>
<td>6.55</td>
</tr>
<tr>
<td>Maximum fuel temperature (°C)</td>
<td>1558</td>
<td>1609</td>
<td>1641</td>
</tr>
<tr>
<td>Time (h)</td>
<td>100</td>
<td>104</td>
<td>105</td>
</tr>
<tr>
<td>Maximum vessel temperature (°C)</td>
<td>486</td>
<td>508</td>
<td>525</td>
</tr>
<tr>
<td>Time (h)</td>
<td>137</td>
<td>141</td>
<td>144</td>
</tr>
</tbody>
</table>

3.3.6 Influence of various core geometrical modifications (radius and height)

The objective of the calculations in this chapter was to evaluate the capability to reduce the size of the core while keeping the same total power and the passive safety features of the annular configurations. As a consequence, calculations were performed taking into account various active core height and internal reflector size. All the results are gathered in Table 3.6 and in Table 3.7. The reduction of the core height from 8 m to 6 m has a strong impact on the maximum fuel temperature due to the increase of the average power density correlated to the decrease of the core height. On the other hand, we also evaluated the impact of the internal reflector size reduction on the maximum fuel temperature. For the calculations, 1 or 2 internal reflector rings are removed from the core. Two solutions were investigated. In the first one, we kept constant the number of fuel rings (3 rings). The consequence is an increase of the average power density due to the reduction of the active zone volume. In the second one, the active zone volume
and the average power density are kept constant. For all the calculations, we kept the number of external reflector rings constant. As we can see in Table 7, the reduction of the internal reflector has a strong impact on the maximum fuel temperature (higher than 200 °C).

Table 3.6: Influence of core height on the maximum fuel temperature achieved during the transient

<table>
<thead>
<tr>
<th>Core height (m)</th>
<th>6</th>
<th>8</th>
</tr>
</thead>
<tbody>
<tr>
<td>Ratio $\frac{H}{D}$</td>
<td>1.59</td>
<td>2.12</td>
</tr>
<tr>
<td>Total core power (MW)</td>
<td>600</td>
<td>600</td>
</tr>
<tr>
<td>Power density (MW.m$^{-3}$)</td>
<td>8.74</td>
<td>6.55</td>
</tr>
<tr>
<td>Maximum fuel temperature (°C)</td>
<td>1780</td>
<td>1558</td>
</tr>
<tr>
<td>Time (h)</td>
<td>96</td>
<td>100</td>
</tr>
<tr>
<td>Maximum vessel temperature (°C)</td>
<td>517</td>
<td>486</td>
</tr>
<tr>
<td>Time (h)</td>
<td>133</td>
<td>137</td>
</tr>
</tbody>
</table>

Table 3.7: Influence of the internal reflector modifications on the maximum fuel temperature achieved during the transient

<table>
<thead>
<tr>
<th>Number of reflector internal rings</th>
<th>Fuel volume (m$^3$)</th>
<th>Total power (MW)</th>
<th>Power density (MW.m$^{-3}$)</th>
<th>Max fuel temperature (°C)</th>
<th>Time (h)</th>
</tr>
</thead>
<tbody>
<tr>
<td>5 (reference case)</td>
<td>91.6</td>
<td>600</td>
<td>6.55</td>
<td>1558</td>
<td>100</td>
</tr>
<tr>
<td>4</td>
<td>91.6</td>
<td>600</td>
<td>6.55</td>
<td>1757</td>
<td>98</td>
</tr>
<tr>
<td>4</td>
<td>80.6</td>
<td>600</td>
<td>7.43</td>
<td>1800</td>
<td>96</td>
</tr>
<tr>
<td>3</td>
<td>91.6</td>
<td>600</td>
<td>6.55</td>
<td>1993</td>
<td>91</td>
</tr>
<tr>
<td>3</td>
<td>64.6</td>
<td>600</td>
<td>9.28</td>
<td>2140</td>
<td>80</td>
</tr>
</tbody>
</table>

3.3.7 Influence of the various reflector materials

In order to decrease the maximum temperature and to favor the evacuation of the residual decay-heat, one of the solutions investigated concerns the replacement of the reflector material (part of the reflector) by a material with better thermal properties. Some of the materials referenced as good candidate for the replacement of the graphite reflector are listed in Table 3.8. The best materials are the material for which the product between $r$ (density) and $C_p$ (specific heat) is the highest. MgO appears as a good candidate. One can notice that the conductivity must be also considered to evaluate the reliability of a material to replace graphite reflector. Various calculations were performed in order
Table 3.8: Physical properties of various material

<table>
<thead>
<tr>
<th>Formule</th>
<th>Nom</th>
<th>Tf  (°C)</th>
<th>M  (g/mol)</th>
<th>Densité (g/cc)</th>
<th>Cp ambiant (J/mol.K)</th>
<th>Cp ambiant (J/kg.K)</th>
<th>ρCp ambiant (J/litre.K)</th>
<th>pCp graph (J/kg graphite)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Ni</td>
<td>Nickel</td>
<td>1456</td>
<td>58,89</td>
<td>8,90</td>
<td>26,1</td>
<td>443</td>
<td>3956</td>
<td>2,22</td>
</tr>
<tr>
<td>Fe</td>
<td>Iron</td>
<td>1538</td>
<td>55,84</td>
<td>7,87</td>
<td>25,1</td>
<td>449</td>
<td>3537</td>
<td>1,98</td>
</tr>
<tr>
<td>MgO</td>
<td>Magnesium oxide</td>
<td>2626</td>
<td>40,30</td>
<td>3,60</td>
<td>37,2</td>
<td>923</td>
<td>3323</td>
<td>1,88</td>
</tr>
<tr>
<td>TiN</td>
<td>Titanium nitride</td>
<td>2950</td>
<td>61,90</td>
<td>5,39</td>
<td>38,0</td>
<td>614</td>
<td>3309</td>
<td>1,85</td>
</tr>
<tr>
<td>Cr</td>
<td>Chromium</td>
<td>1907</td>
<td>52,00</td>
<td>7,15</td>
<td>23,4</td>
<td>450</td>
<td>3218</td>
<td>1,80</td>
</tr>
<tr>
<td>V</td>
<td>Vanadium</td>
<td>1910</td>
<td>50,94</td>
<td>6,00</td>
<td>24,9</td>
<td>489</td>
<td>2933</td>
<td>1,64</td>
</tr>
<tr>
<td>C</td>
<td>Carbon (graphite)</td>
<td>4549</td>
<td>12,01</td>
<td>3,513</td>
<td>6,1</td>
<td>508</td>
<td>1784</td>
<td>1</td>
</tr>
</tbody>
</table>

to evaluate the influence of MgO. The results are gathered in Table 3.9. By replacing the inner reflector and one of the outer rings, the maximum fuel temperature is reduced by 40 °C. The influence of the reflector replacement was also evaluated with respect to the maximum fuel temperature achieved during normal operation. It showed that the reflector made with MgO has a neutronic impact (reduction of the spectrum transient at the interface between core and reflector) and reduces the maximum fuel temperature by 50 °C during normal operation. Some materials listed in Table 3.8 were also evaluated.

Table 3.9: Influence of MgO as reflector material on the maximum fuel temperature

<table>
<thead>
<tr>
<th>Number of MgO rings in the inner reflector</th>
<th>3</th>
<th>5</th>
</tr>
</thead>
<tbody>
<tr>
<td>Number of MgO rings in the outer reflector</td>
<td>0</td>
<td>1</td>
</tr>
<tr>
<td>Maximum fuel temperature (°C)</td>
<td>1553</td>
<td>1543</td>
</tr>
<tr>
<td>Time (h)</td>
<td>104</td>
<td>101</td>
</tr>
<tr>
<td>Maximum vessel temperature (°C)</td>
<td>472</td>
<td>483</td>
</tr>
<tr>
<td>Time (h)</td>
<td>156</td>
<td>136</td>
</tr>
</tbody>
</table>

The results are gathered in Table 3.10 and in Figure 3.9. As can be noticed, Chromium is better than Mgo and the maximum temperature is significantly reduced. This trend is mainly due to the better thermal properties of chromium at high temperature.

Table 3.10: Influence of various reflector materials on the maximum fuel temperature

<table>
<thead>
<tr>
<th>Material</th>
<th>Chromium (Cr)</th>
<th>Vanadium (V)</th>
<th>MgO</th>
</tr>
</thead>
<tbody>
<tr>
<td>Maximum fuel temperature (°C)</td>
<td>1437</td>
<td>1552</td>
<td>1553</td>
</tr>
<tr>
<td>Time (h)</td>
<td>81</td>
<td>98</td>
<td>104</td>
</tr>
<tr>
<td>Maximum vessel temperature (°C)</td>
<td>473</td>
<td>489</td>
<td>472</td>
</tr>
<tr>
<td>Time (h)</td>
<td>118</td>
<td>130</td>
<td>156</td>
</tr>
</tbody>
</table>
3.4 Synthesis and conclusion

The objective of the analysis was to evaluate the influence of some design parameters on the passive safety features of the VHTR 600 MWth annular core configuration. Three major modifications were analyzed. It concerns the average power density, the core height and the coolant flow through the core. Moreover, we also investigated the impact of the reflector material. The results showed that the passive safety features of the annular core configuration are strongly affected by the increase of the average power density. The increase of the active core height is less damaging for both nominal operation and transient scenario. After all, the replacement of the inner reflector and part of the external reflector by material with better thermal properties (MgO) allows to gain 40 °C on the maximum fuel temperature.

3.5 Bibliography

References

Chapter 4

HTR Decay Heat Removal in a prismatic VHTR including heat transfers through the reactor pit (EDF R&D)
Summary

A high temperature reactor (HTR) is based upon a massive graphite core and a flow of helium which acts as coolant. One interesting safety-related feature of an HTR is its passive heat removal capability in case of loss of a cold sink. Previous studies done at Areva-NP and EDF on a prismatic block concept have investigated depressurized conduction cooldown as well as pressurized cases. Areva-NP had done its investigations on a limited angular sector (sector of 30°) while EDF had simulated half a HTR including the hot duct. However, in both studies, boundary conditions were prescribed directly on the external side of the vessel using correlations. The purpose of this paper is to present exploratory studies including thermal heat transfers taking place through the reactor pit, using the CFD code Code_Saturne coupled with the thermal code SYRTHES. Code_Saturne is a general purpose CFD finite volume code which handles the flow convective aspects, while SYRTHES is a taking care of conduction inside solid parts and wall to wall radiation using respectively a finite element method and a radiosity approach.

A flexible numerical technique has been used. It is based upon the coupling at each time step during the thermal transient between one instance of the thermal code SYRTHES and two different instances of the CFD code Code_Saturne. The first one is in charge of the helium flow inside the vessel while the second takes into account the air natural convection flow which develops inside the reactor pit. Similarly to previous studies, all prismatic graphite blocs are represented but an homogeneous approach is retained to model the fuel blocs forming the annular core in which the helium flows. The influence of several parameters on the temperature reached in different parts is investigated.

Calculations presented are not design calculations but remains general exploratory computations and explain the numerical methodology retained at EDF to handle some decay heat removal aspects. Likewise, the paper underlines that such a numerical approach could be applied to other type of reactors like Sodium Fast Reactor (SFR) or Gas Fast Reactor (GFR) where the decay heat removal may also turn out to be a challenging task.

Keywords

HTR, Decay Heat Removal, Thermal Hydraulics, Conjugate Heat Transfer
4.1 Introduction

A High Temperature Reactor (HTR) is based upon a massive graphite core and a flow of helium which acts as coolant. One interesting safety related feature of an Very High Temperature Reactor is its passive heat removal capability in case of loss of the main cold sink. This problem has already been investigated by [4-1] [4-2], on a limited angular sector (30°) for the AREVA-NP prismatic ANTARES design and by EDF [4-3] on half geometry. In both studies, the reactor pit was not accounted for. Here the same kind of reactor is revisited on half a geometry including the hot gas duct. A three dimensional numerical model has been set in which conduction, radiation and convection are accounted for simultaneously. The unstructured finite volume CFD code Code_Saturne calculates the convective part while the finite element thermal code SYRTHES handles conduction and radiation heat transfers. A nominal condition has been calculated and from this nominal state a depressurized conduction cooldown transient is calculated over a duration of about 100 hours. One underlines that computations presented in this paper are not precise design calculations but remain exploratory calculations. The purpose of this paper is also to present a possible methodology to handle decay heat removal which could, after adaptation, be applied to other kinds of reactors like Gas Fast Reactor (GFR) or even Sodium Fast Reactor (SFR).

4.2 Description of the reactor Core

The ANTARES concept developed by AREVA-NP consists in a core made of 1020 hexagonal graphite blocks. The fuel is located in 102 columns of ten blocks arranged in an annular shape. The inner and outer reflectors are also made of prismatic blocks, but those ones are full graphite blocks. The reactor key parameters which have been used in the present calculations are in Table 4-I.

<table>
<thead>
<tr>
<th>Reactor key parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Core thermal power</td>
<td>600 MW</td>
</tr>
<tr>
<td>Primary helium flow rate</td>
<td>226 kg/s</td>
</tr>
<tr>
<td>Primary helium pressure</td>
<td>72 bar</td>
</tr>
<tr>
<td>Primary helium inlet/outlet temperature</td>
<td>490 ºC</td>
</tr>
<tr>
<td>Reflector outer radius</td>
<td>3.4 m</td>
</tr>
<tr>
<td>Core inner/outer radius</td>
<td>1.48 m/2.42 m</td>
</tr>
<tr>
<td>Core height</td>
<td>8 m</td>
</tr>
<tr>
<td>Vessel outer radius</td>
<td>3.785 m</td>
</tr>
</tbody>
</table>
The core is held by a steel core barrel and the structures are enclosed in a main vessel which is connected to a heat exchanger by a cross vessel. Fig. 4-1 shows a vertical section of the reactor. At this stage, the geometry seems too complex to be exactly represented. Therefore a certain number of geometrical approximations have been made. In particular (from top to bottom):

- The holes and control rods are removed,
- The fins are not taken into account,
- The fuel blocks and their cooling channels are treated by an homogenous model,
- The riser is considered annular,
- The horizontal gaps between blocks are removed,
- Complex passages in the bottom part of the core are not accounted for,
- The outlet plenum is simplified,
- The complex mixing devices made of plates with holes are not represented,
- The Shutdown Core Cooling System (SCCS) is not modeled

On the other hand, the vertical gaps between each core block are explicitly represented (even if local heat transfer coefficients as well as pressure drop are modeled by correlations, i.e the boundary layers are not explicitly modeled).

Fig. 4-2 shows the geometry calculated in which geometrical approximations have been introduced. At nominal operating conditions, the cold helium (490°C) enters the reactor.
through the outer annulus of the cross-vessel, goes down through a mixing device where it cools the metallic support structures, before going up flowing through the riser. The purpose of this flow between the core and the pressure vessel is to cool down the pressure vessel. Then it arrives in the upper plenum before flowing downwards through the annular fuel blocks where it is heated and finally collected in a plenum located below the core before exiting through the inner annulus of the cross-vessel to the IHX vessel. One part of the primary helium flow bypasses the fuel blocks through the vertical gaps (2mm wide) existing between the blocks. In reality, the width of these gaps is likely to vary with temperature and irradiation, but such a variation is not considered here. Inside the annular fuel blocks (yellow color inside the core), a source term corresponding to the fuel heat deposit is prescribed.

4.3. Equations solved

In this simulation, conduction, convection and radiation heat transfer must be solved simultaneously. The model used and equations solved are described thereafter.

4.3.1 In the fluid – the CFD code Code_Saturne

The EDF finite volume CFD code, Code_Saturne, is used to solve Navier-Stokes equations on unstructured meshes. The flow is assumed Newtonian and the density is only a function of temperature. Indeed, even if the velocity in nominal condition is high (up to 70m/s), it is still much lower than the speed of sound in helium at 72 bars (about 1500m/s). The filtered Navier-Stokes equations can be written (the filtering operator is omitted for the sake of clarity):
\[ \frac{\partial \rho}{\partial t} + \frac{\partial \rho U_j}{\partial x_j} = 0 \] (4-1)

\[ \rho \frac{\partial U_i}{\partial t} + \rho U_j \frac{\partial U_i}{\partial x_j} = -\frac{\partial p^*}{\partial x_i} + \frac{\partial}{\partial x_j} \left[ \mu \left( \frac{\partial U_i}{\partial x_j} + \frac{\partial U_j}{\partial x_i} \right) - \frac{2}{3} \frac{\partial}{\partial x_i} \left( \mu_e \frac{\partial U_j}{\partial x_j} \right) \right] + (\rho - \rho_v) g_i \]

In Eq. 4-1, \( U_i \) are the filtered components of the velocity, \( p^* \) stands for the pressure (minus the reference hydrostatic pressure), \( \mu_e \) represents \( \mu + \mu_t \) where \( \mu \) and \( \mu_t \) are respectively the molecular and turbulent viscosities. Turbulence is handled through a classical \( k-\varepsilon \) model. Code_Saturne is an open source code, more details on numerical aspects of Code_Saturne can be found in [4-4], [4-5] and further information can also be found on the web site http://rd.edf.com/code_saturne.

Numerical Technique Used for Solving the Fluid Equations

In the collocated finite volume approach used in Code_Saturne, all variables are located at the centres of gravity of the cells (which may take any shape). The momentum equations are solved by considering an explicit mass flux (the three components of the velocity are thus uncoupled). Velocity and pressure coupling is ensured by a prediction/correction method with a SIMPLEC algorithm [4-6]. The Poisson equation is solved with a conjugate gradient method. The collocated discretization requires a Rhie and Chow [4-7] interpolation in the correction step to avoid oscillatory solutions.

4.3.2 Conduction in the solid – SYRTHES

The EDF solid code SYRTHES relies on a finite element technique to solve the general heat equation where all properties can be time, space or temperature dependent.

\[ \rho C_p \frac{\partial T}{\partial t} = \frac{\partial}{\partial x_j} \left( k \frac{\partial T}{\partial x_j} \right) + \Phi, \] (4-2)

In Eq. 4-2, \( T \) is the temperature, \( t \) the time, \( \Phi \), a volume heat source or sink, \( \rho \) and \( C_p \), respectively the density and the specific heat. \( k \) (a matrix when the material is anisotropic) designates the conductive behaviour of the medium. For optimization reasons, only two kinds of elements have been retained (triangles in 2D, tetrahedra in 3D). SYRTHES has been checked thoroughly against analytical test cases. SYRTHES is also becoming an open source code, more details on the possibilities of the finite element code SYRTHES can be found in [4-8] and [4-9] and soon on http://rd.edf.com.

4.3.3 Heat Transfer at the Fluid/Solid Interface

At the interface, every time step, the thermal coupling is performed. This coupling relies on an iterative procedure. Let \( T_s \) be the temperature of an internal solid node, \( T_w \) the temperature at a node which belongs to the interface, and \( T_f \) the temperature of a fluid point (located generally in the log layer). At time \( t^{(n)} \), the CFD tool Code_Saturne provides after calculation:
- \( h^{(n)} \): the local heat exchange coefficient at time \( t^{(n)} \)
- \( T_f^{(n)} \): the local inside fluid temperature at time \( t^{(n)} \)

using these data, the flux to be applied to the solid is:
Then, using this flux or the exchange conditions \( h^{(n)} \) and \( T_f^{(n)} \), SYRTHES can solve the heat conduction equation inside the solid. This gives an updated temperature over all the solid region. These new values \( T_s^{(n+1)} \), are also updated on the boundary. Therefore \( T_w^{(n+1)} \) is also known and the iterative procedure may keep going on.

### 4.3.4 Heat Transfer in the porous media

As said previously, the fuel blocks are modelled using an homogeneous model in which solid and fluid cells overlap. The volume heat flux exchanged between the helium flowing in the channels and the solid matrix is prescribed by a relation of the type \( h_{\text{vol_eq}} (T_f - T_s) \) where \( T_f \) and \( T_s \) are respectively the fluid temperature of the porous medium and \( T_s \) the corresponding solid cell. \( h_{\text{vol_eq}} \) is a correlation found either by literature correlation (Colburn) or by a fine local calculation. Homogeneous solid properties have been provided par AREVA-NP from detailed calculations, as it has been reported in references [4-1] and [4-2].

### 4.3.5 Radiation from wall to wall – SYRTHES

Helium is a simple gas which is not a participating medium from the thermal radiation point of view. Radiative heat transfer takes place only from wall to wall. We consider that all walls behave in a diffuse and isotropic manner. Moreover, wall properties are considered grey which means that they can be defined with a simple emissivity and that emissivity \( \varepsilon \) is equal to absorbtivity (Kirchoff law). The approach used is to rely on the radiosity approach (see [4-10]). Radiosity \( J \) is the rate at which radiant energy streams away from a point.

In order to solve numerically this equation, we break down the surface \( S \) into a finite number of patches, and to further simplify the problem, all properties and values across each patch are considered constant. One purely geometrical quantity appears, the view factor \( F_{ij} \), which represents the proportion of the total power leaving patch \( S_i \) that is received by patch \( S_j \).

![Fig. 4-3: Radiation exchange between surface dS1 and dS2](image)

\[
F_{12} = \frac{1}{S_1} \int_{S_1} \int_{S_2} \frac{\cos \theta_1 \cos \theta_2 dS_1}{\pi l^2} dS_2
\]  

(4-4)
If we consider a closed domain, for a patch \( i \) of surface \( S_i \) surrounded by \( N \) patches \( S_j \), the equation (4-5) can be written down, \( E_i \) the emission of a patch \( i \) and \( J_j \) the radiosity of a patch \( j \).

\[
E_i S_i = \sum_{j=1}^{n} S_j R_{ij} J_j
\]  

(4-5)

After rearrangement, we arrive to the system of Eq. 4-6, which is solved by a classical conjugate residual method.

\[
J_i - \rho_i \sum_{j=1}^{n} F_{ji} J_j = M_i
\]  

(4-6)

This approach has many advantages but has the drawback to be quite costly in terms of computing resources due to the fact that the number of view factors is dependent of the square of the number of independent faces. Here due to the huge number of wall faces induced by the gaps between blocks, it would lead to too heavy calculations, even if the radiation grid is independent of the conduction grid. To cope with this problem, we have decided to handle thermal radiation through gaps (and more generally between two walls facing each other with a small gap between them, like for example inside the riser, with respectively a temperature \( T_1 \) and \( T_2 \) and emissivities \( \varepsilon_1 \) and \( \varepsilon_2 \)) by a contact resistance (Eq. 4-7) so that the phenomenon stays differential instead of integral. Otherwise the complex radiation model applies.

\[
\varphi = \varepsilon_1 \varepsilon_2 \frac{(T_1^4 - T_2^4)}{1 - (1 - \varepsilon_1)(1 - \varepsilon_2)\sigma}
\]  

(4-7)

The global thermal radiation handling results from the combination of these two complementary radiation approaches.

### 4.3.6 Physical properties

The fluid physical properties of helium and air are modeled as that of an ideal gas. However, conductivity, molecular viscosity or specific heat are considered to be temperature independent. All other physical properties are considered as constant, but the density variation in the two gases is as follows:

\[
\rho_{\text{helium}} = \frac{1}{(0.078857 + 0.00028869 T)} \quad \text{(kg/m}^3\text{)}
\]  

(4-8)

\[
\rho_{\text{air}} = 1.2302 - 0.0034794 \ T - 5.5423 \times 10^{-6} \ T^2 - 3.3972 \times 10^{-9} \ T^3 \quad \text{(kg/m}^3\text{)}
\]  

(4-9)

Regarding the solid properties, steel components are considered also to be temperature independent with a constant emissivity of 0.6. Another important property is the graphite block emissivity. This value is also considered to be constant but with a value of 0.9 (a sensibility study with an emissivity of 0.6 is also presented in this paper).

### 4.3.7 Initial and boundary conditions

At the start of the nominal calculation, a 490°C temperature is considered throughout
the domain. For Decay Heat Removal calculations, the thermal results of the nominal calculation are used. Unlike in previous studies [4-1], [4-2] and [4-3], the boundary conditions set on the external side of the main vessel are not necessary any more since they are taken into account through the thermal coupling. On the external side of the concrete the following heat exchange condition has been set (T=30°C and h=10 W/m²/°C) (see [4-11] for more details). One underlines that this boundary condition, being far from the zone of interest, is likely to have a very small influence on the thermal behaviour of the core and the main vessel.

Regarding the power distribution we consider that the reactor is operating at full power (i.e 600MW), and that the RCCS is in operation. The power generated in the annular core is considered as uniformly distributed in the radial direction. The axial power profile shown on Fig. 4-4 (left). When calculating Depressurized Core Cooling transient, the primary helium flow is stopped and the rods are dropped. During the transient, the decay heat is still dissipated in the core. It begins at 5% of the full power and reduces quickly as shown on Fig. 4-4 (right).

![Decay heat power (% of the nominal power) vs. Time in hours](image)

**Fig. 4-4 : Normalized axial power factor and Decay heat power (in % of nominal power)**

### 4.4. Grids generated

In order to use the general purpose numerical tools *Code_Saturne* and SYRTHES, grids are needed. In this calculation three independent grids approaching the same interface have been generated:

- A grid for conduction,
- A grid for radiation,
- A grid for CFD calculation.

Due to the geometrical complexity, a commercial grid generator (here the software Simail) has been used. It is a general purpose grid generator in which it is possible to build parametric meshes. It should be underlined that these three grids approach the same interface but do not need to have the same spatial discretization at the interface. This provides a good flexibility for such geometrically complex cases.
4.4.1 The conduction mesh

The conduction mesh is to be used by the finite element thermal code SYRTHES. It is composed exclusively of tetrahedra and its generation turned out to be a quite challenging task. All individual blocks are taken into account and are separated by the 2mm wide vertical gaps. Likewise, a detailed description of the hot gas duct connection has been included.

The conduction mesh contains around 15.9 millions linear tetrahedra and 3.07 millions nodes. Each element has a color in order to easily set the different material properties. Likewise, faces are colored so that it becomes easy to set boundary conditions. As can be seen on Fig. 4-5, even with geometrical approximations, the mesh remains quite complex.

Fig 4-5 : Detailed view of the solid mesh (top and bottom part of the reactor)
4.4.2 The radiation mesh

In a High Temperature Reactor, thermal radiation heat transfers are of primary importance due to the high temperatures likely to be present. Moreover, helium is a non-participating medium therefore wall to wall radiation heat transfer takes place. The approach used by SYRTHES for this particular calculation is based on a mixed approach.

![Radiation Mesh Diagram]

1D approach

3D radiosity approach

Global radiation

Fig. 4-6: Details of the radiation meshes (wall to wall + radiosity = complete radiation)

Where complex shadowing is taking place (middle view on Fig. 4-6), the general radiosity algorithm implemented in SYRTHES is used, while radiation contact resistance (left) is used for regions where it applies. The total radiation mesh (right) is a combination of the two. These complementary approaches allow us to stay under around 650 independent radiation faces for half a HTR geometry, which remains reasonable to handle.

4.4.3 The two convection meshes (air cavity and helium circuit)

These meshes are to be used by the finite volume CFD code *Code_Saturne*. They are completely independent and can be refined independently. This flexibility has been used in this study to refine the mesh used in the air cavity without affecting the mesh used for the helium flow.

This inner unstructured fluid mesh is made up of 754,008 cells mainly composed of bricks and prisms. It is worth underlying that *Code_Saturne* allows hanging nodes. This feature brings a much appreciated flexibility in the present calculation. This mesh has been quite difficult to generate and induce some challenge in terms of mesh distortion as pointed out in Fig. 4-7 due to the presence of the gaps between the graphite blocks.
Fig. 4-7: Details of the inner fluid mesh (helium circuit)

Fig. 4-8: Details of the fluid meshes used for the reactor cavity.
Fig. 4-7 illustrates the high distortion expected in the cells of the fluid mesh to take into account very different spatial scales (the core is 8 meters high, and the gaps between graphite blocks are just 2mm wide)

Regarding the fluid reactor cavity mesh (see Fig. 4-8), two refinements have been tested: a coarse one with only about 100,000 cells and a more refined one with 448,416 hexahedra. All results presented in this paper have been computed with the refined mesh.

4.5 Numerical methodology used to handle multi-physical problems

When including the convection reactor cavity, several approaches are possible. The first one (tested initially at EDF) is to set up a unique mesh for both the inner part and the reactor cavity and specify an adequate density variation. Even if possible, this approach turned out not to be optimal. Indeed in the present case, one flow is helium pressurized at 72 bar and characterized by very high velocities while the other one is air at one bar and natural convection characterized by fairly low velocities.

Past experiments done at EDF had shown the interest of coupling the thermal code SYRTHES with several CFD codes (see [4-12] for a coupling of SYRTHES with a finite element CFD code and a finite volume code). If necessary, this provides some flexibility to activate different turbulent models in order to simulate very different flow regimes. More generally, in the case of sodium fast reactor, the fluid inside being a liquid metal, its thermal behavior at the wall is quite different from a gas flow. Sometimes one of the flow is a one phase flow while the other one is a two phase flow like in heat-exchangers. In some cases, taking into account simultaneously a compressible and a non compressible flows separated by a wall (like in turbine blade cooling) would be of interest.

The same technique has been implemented to perform a coupling between several SYRTHES and several instances of the CFD code Code_Saturne. It is used for the first time in the present case with only one SYRTHES and two Code_Saturne.

Each Code_Saturne identifies its part of the wall coupled with the solid code, adds the corresponding contributions in a global table containing the complete information at the fluid/solid boundary interface. These boundary information are then sent to SYRTHES which uses them to obtain an updated thermal field throughout the solid. Then the global wall temperature at the interface is dispatched back to the adequate fluid code as shown on the sketch of Fig. 4-9.
Fig. 4-9: Data exchange between the solid code and several fluid codes

Here Code_Saturne being used on a parallel computer, it was even possible to obtain a fairly well distributed load balance from the CPU point of view. For the particular case treated here, 32 processors are devoted to the inner helium flow, 15 processors are used for the cavity air flow and one processor is dealing with the solid conduction and radiation. One time step takes from 50s to 20s when a converged state has almost been reached.

4.6. Results of nominal configurations

4.6.1 Nominal case

We start the calculation by setting a constant temperature of 490°C throughout the entire domain. Then, a transient calculation is performed. After a long time, a steady state is reached. The very large time scales present in the case obliged us to use acceleration methods. First, due to the combination of high velocities and small cells, the dynamic time step in the helium flow has to be kept small (around $5 \times 10^{-4}$s) in order to meet some numerical requirements regarding the Courant number. Obtaining a thermally converged state with such a small time step is not realistic. To overcome this difficulty, in Code_Saturne, we apply a multiplying coefficient only on the temperature equation in order to reach a “temperature time step” of 10s. The same procedure is used for the cavity flow. This possible because the velocity field is not changing much over time. A time step of 10s is not inducing any numerical problem in the solid.

Even with this procedure, this turned out to be insufficient in order to obtain in a reasonable time a converged state in the concrete part. Indeed, the concrete walls are very thick (up to 2m wide) and characterized by a fairly low conductivity (1.5 W/m°C). To accelerate the convergence, it has been decided to reduce the concrete heat capacity by a factor 1000 in a first phase, before setting back its original value. In that phase, we are interested by a converged state, therefore changing the inertia of the concrete has little consequences. The core temperature distribution is very similar to what had been
obtained in previous studies (see [4-1] [4-2] and [4-3]). The field is characterized by a strong dissymmetry with the hot temperature region located at the bottom of the core due to the convective effect even if the maximum deposit is located at the center. As can be seen on the thermal field inside the concrete, the temperature is higher where the RCCS device is not present, i.e above a certain location and in the bottom part of the reactor cavity.

Fig. 4-10 : Temperature fields (in °C) obtained in the air cavity, solid and helium (temperature scales are adapted to each domain)

The results obtained suggest that taking into account accurately the radiation heat transfer through the reactor cavity, as well as modeling the natural convection air flow inside the cavity is not influencing much the maximum temperature reached inside the fuel blocks. It is however interesting to describe the air flow behavior inside the reactor cavity. Due to the RCCS device which keeps the temperature of the side wall close to 65°C thanks to pipes in which cold water is circulating, a natural convection loop starts as shown on Fig. 4-11 (top left).

The air in contact with the RCCS is colder and heavier, therefore it goes down, while the air in contact with the main vessel is heated and goes up. It is partly blocked by the two flanges at the top of the vessel as shown on Fig. 4-11 (top right). The warm air goes up to the point where the RCCS stops. There a thermal stratification is taking place.

A particular flow also develops at the bottom of the reactor. The cold air flowing down along the RCCS device impinges against the horizontal concrete support and goes up above the anchor. However this anchor has a very different height where the cross-vessel is located. This provides a privileged path for the cold and therefore heavier air. With an adequate temperature scale, Fig. 4-11 (bottom) shows this behavior on the air temperature. This of course induces also a slightly non symmetrical temperature distribution of the main vessel as is shown on Fig. 4-12 (left). The cold part is due to the RCCS device running along the concrete wall only on a restricted vertical portion as
indicated on Fig. 4-12 (right).

Fig. 4-11: Velocity fields in the bottom of the reactor cavity (influence of the cross vessel)
4.6.2 Influence of the main nominal flow rate

Three calculations have been performed with three different flow rates:

- The nominal flow rate: 226 kg/s,
- A flow rate reduced to 80% of the nominal flow rate,
- A flow rate increased to 120% of the nominal flow rate.

The same qualitative behavior is observed in all cases. To underline the fact different
temperature scales are applied for each case. As shown on Fig. 4-13, the less the flow rate, the higher the temperature inside the fuel blocks. The maximum temperature reached in the homogeneized fuel blocks are respectively:

- 1023 °C for a nominal flow rate of helium of 226 kg/s,
- 1155°C for the case with a flow rate reduced to 80%
- 935°C for the case with a flow rate increased up to 120%

Regarding the fluid temperature, Fig. 14 shows the influence on the helium temperature. The average outlet temperature for each case is of the order of:

- 985 °C with a nominal flow rate of helium of 226 kg/s,
- 1105 °C with a flow rate reduced to 80% of the nominal flow rate,
- 903 °C with a flow rate increased to 120% of the nominal flow rate.

As already pointed out in previous studies (see [4-3]), the temperature distribution is fairly symmetrical, even with the presence of a cross vessel only located on one side of the reactor.

4.7 Decay Conduction Cooling (DCC) Calculations

High Temperature Reactor relies on a vessel which is not insulated, in order to passively remove the heat from the steel reactor vessel to the reactor cavity. To increase this cooling effect a Reactor Cavity Cooling System (RCCS) is installed along the reactor cavity walls (see Fig. 4-12). It also helps to maintain the concrete below prescribed
temperatures. This device is activated during normal operation, but also during an emergency cooldown to help maintaining the fuel, the reactor vessel, and more generally all steel structures as well as the cavity concrete within acceptable limits. Most of the heat is transferred across the cavity by thermal radiation and partly by natural convection. Previous studies done at EDF and Areva-NP (see [4-1] [4-2] [4-3]) used a heat transfer correlation directly applied to the reactor vessel. Here a more refined calculation takes into account the complex radiation heat exchange taking place across the cavity.

4.7.1 Description of the thermal behavior during the DCC transient.

An upward migration of the hot spot is observed. Initially located at the bottom of the core, the maximum temperature goes up towards the center of the core where the heat deposit is at its maximum. A decrease in temperature is observed in the upper and lower part of the reactor, the cooling of the RCCS being considered still active. The maximum temperature rises up to about 1400°C in the fuel blocks.

Fig. 4-15 : Solid temperature fields (in °C) of a DCC transient (from 0h to 100h)
4.7.2 Influence of different physical parameters

Exploratory studies have been performed to understand the influence of several parameters on the temperature distribution. Even if temperature is available at all location, in order compare the different cases, a few “numerical thermal probes” have been used. They are shown on Fig. 4-16.

Fig. 4-16 : Position of the thermal probes (symmetry plane and z=6.645m)

Fig. 4-17 : Temperature evolution at node 88224 (near location of max fuel T)
Emissivity of the graphite = 0.6 instead of 0.9

Emissivity of the graphite = 0.9

Fig. 4-18: Temperature evolution at node n90224 (center of the inner reflector)

Graphite conductivity multiplied by 1.2

No RCCS

Fig. 4-19: Temperature evolution at node n258894 (external side of the main vessel)
The curves presented in Fig. 4-17 to Fig. 4-20 provide preliminary explanations regarding the influence of some parameters.

**Influence of the reactor cavity emissivity**
The influence of emissivity on the radiative heat exchange taking place inside the reactor cavity has been investigated. Two emissivities have been used (0.8 and 1.). It turns out that (at least during the first 100h, this parameter seems to have a relatively small influence on the maximum temperature reached by the fuel blocks (Fig. 4-17). On the other hand, it has a bigger impact on the main vessel temperature (Fig. 4-20). The decrease of temperature at the beginning of the transient for the curve ($\varepsilon=1.$) can be explained by the fact that in the present calculation the DCC transient starts from a nominal case where the emissivity is of the order of 0.8 instead of 1.

**Influence of the emissivity of the graphite blocks (between gaps)**
In these exploratory calculations, the influence of emissivity of the graphite block has also been tested. Reducing the emissivity from a value of 0.9 to 0.6 has a fairly strong influence on the fuel temperature due to the fact that the energy released in the fuel blocks escapes less easily radially (Fig. 4-18).

**Influence of the graphite physical properties (conductivity of the graphite)**
Likewise, as indicated in [4-2], the conductivity taken for the graphite block is likely to be smaller than in reality for graphite aging reasons. A calculation where this value has
been arbitrarily multiplied by a factor 1.2 has been performed. In that case the heat propagates more easily through the graphite and reduces the peak temperature.

Configuration without RCCS

The last calculation presented corresponds to a case where even the RCCS device is not active any more (even if this case is very unlikely). In that case, no cold sink is present inside the cavity reactor. The temperature increases continuously but temperature inertia as well as conduction and radiation phenomena are still affecting the temperature redistribution. At least during the first 100h of the DCC transient, it worth noting that regarding the fuel maximum temperature, the difference stays fairly small (around 20°C) between a case when the RCCS is active and when it is not considered (Fig. 4-17). On the other hand the vessel temperature as well as the concrete see a large temperature rise (Fig 4-20).

![ Thermal distribution after 100h (when the RCCS is not activated) (Temperature in °C) ](image)

4.8. Conclusion

Exploratory calculations have been performed on a complex half HTR geometry including the heat transfers taking place through the reactor cavity. The temperature distribution and behavior confirms what had been found in previous work done at Areva-NP and EDF on the ANTARES High Temperature Reactor Design. The maximum temperature of fuel blocks in nominal condition as well as in Depressurized Core Cooling transient are coherent with previous studies performed on a
reduced angular sector or which did not take into account the reactor cavity.

Several calculations have shown, as expected, the strong influence of the main flow rate on the temperature distribution. The qualitative shape of isothermal lines is very similar, but the maximum temperature of the helium at the outlet varies from 900°C to 1100°C according the flow rate retained in the range [-20%,+20%] around the nominal value of 226 kg/s.

The influence of the emissivity of the reactor cavity has also been investigated. It turns out that at least during the first 100h of the DCC transient, it modifies only very slightly the maximum temperature reached inside the fuel blocks. Typically, the limit case when the RCCS is not active any more (unlikely situation) induce only a temperature increase of 20°C. On the other hand, the emissivity has a larger impact on the vessel temperature.

Regarding the material properties, the impact of a change in the graphite conductivity or the graphite emissivity has been investigated. Since it affects directly the core itself, the maximum temperature observed in the fuel block is strongly impacted by these parameters. As expected, a reduction in the emissivity induces a higher peak temperature. On the other hand an increase in the conductivity reduces the peak temperature.

Using the same meshes and methodology Pressurized Core Cooling (PCC) transient could also be calculated. As pointed out in [4-2] and [4-3], this thermal transient is however quite complex to manage due to the fact that convection has to be taken into account in a very complex geometry for a very long time.

The numerical procedure presented in this paper uses a coupling between one SYRTHES code (handling the conduction in solids as well as wall to wall radiation) and two instances of the CFD code Code_Saturne: the first handles the high velocity pressurized helium flow taking place inside the vessel, while the second Code_Saturne takes care of the natural convection in the air of the cavity. The thermal coupling between all the codes takes place at each time steps. This methodology is interesting because it provides a nice flexibility both in terms of numerical and physical point of view.

- Physically, in the present case, it allows to modify very easily the spatial discretization of one fluid domain without impacting the other one. It allows, if necessary, to choose fluid with very different physical properties (like sodium/air, water/air, etc…), different turbulent models or wall laws in each fluid domain. Typically, at EDF, SYRTHES being also coupled with the two phase flow code NEPTUNE_CFD, it allows calculations of complex configurations mixing one phase flow and two phase flow problems separated by walls, like in some heat-exchangers.

- Numerically, compared to a more classical approach where all fluid domains would be solved with only one fluid mesh and one CFD code where fluid properties would be changed, here each system is better conditioned. Moreover, the precision of the solver can be adapted to the domain treated. Such a
numerical advantage is likely to be even bigger if one fluid is a liquid and the other one is a gas with very different pressure variations.

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Nomenclature
\[ \rho \] : density
\[ U_i \] : velocity component
\[ p^* \] : pressure
\[ g \] : gravity
\[ x_i \] : coordinates
\[ t \] : time
\[ \mu \] : viscosity
\[ C_p \] : specific heat
\[ k_s \] : thermal conductivity
\[ \phi \] : volume heat source
\[ h \] : heat transfer coefficient
\[ T \] : temperature
4.9 Bibliography

References


[4-7] C.M. RHIE, W.L. CHOW, “A numerical study of a turbulent flow past an isolated airfoil with training edge separation”, AIAA paper, 82-0998


