



The effect of fuel thermal conductivity on the behavior of LWR cores during loss-of-coolant accidents [☆]



Kurt A. Terrani ^{a,*}, Dean Wang ^a, Larry J. Ott ^a, Robert O. Montgomery ^b

^a Oak Ridge National Laboratory, Oak Ridge, TN 37831, USA

^b Pacific Northwest National Laboratory, Richland, WA 99354, USA

ARTICLE INFO

Article history:

Available online 5 October 2013

ABSTRACT

The effect of variation in thermal conductivity of light water reactor fuel elements on core response during loss-of-coolant accident scenarios is examined. Initially, a simplified numerical analysis is utilized to determine the time scales associated with dissipation of stored energy from the fuel into the coolant once the fission reaction is stopped. The analysis is then followed by full reactor system thermal-hydraulics analysis of a typical boiling and pressurized water reactor subjected to a large break loss-of-coolant accident scenario using the TRACE code. Accordingly, sensitivity analyses to examine the effect of an increase in fuel thermal conductivity, up to 500%, on fuel temperature evolution during these transients are performed. Given the major differences in thermal-hydraulics design aspects of boiling and pressurized water reactors, different fuel and temperature responses during the simulated loss-of-coolant transients are observed.

© 2013 Elsevier B.V. All rights reserved.

1. Introduction

A light water reactor (LWR) core is comprised of hundreds of fuel assemblies consisting of a two-dimensional array of rods roughly 4 m in length. In the vast majority of commercial reactors, the fuel rods contain uranium fuel pellets inside a zirconium alloy tube [1,2]. Under normal operation, the fission heat generated inside uranium is transported within the oxide pellet, across the fuel-cladding gap and the zirconium alloy cladding, and subsequently to the flowing water that acts as the coolant as well as the moderator. Total power output of the reactor is proportional to the power generated in the fuel, which could be limited by a number of design constraints pertaining to steady-state and transient operating conditions [3–6]. Briefly, for steady-state conditions, design constraints aim to limit damage to the fuel rod by limiting the maximum heat flux from the cladding to the coolant, fuel centerline temperature, and the extent of vibration and wear. An increase in fuel power beyond the critical heat flux (design limit) results in departure from nucleate boiling (DNB) in pressurized water reactors (PWRs) or dryout conditions in boiling water reactors (BWRs). Under transient conditions, the design limits are put in place to limit core damage and maintain a

controllable and coolable geometry in the core under design-basis-accident (DBA) scenarios [7]. For a loss-of-coolant accident (LOCA), the design criteria limits the peak cladding temperature to below 1204 °C [8] as well as the maximum thickness of the cladding converted to oxide as a result of reaction with steam [9]. For a reactivity insertion accident scenario, the maximum energy deposited in the fuel is limited to avoid fuel rod failure due to excessive heat flux or pellet cladding mechanical interaction [7,10]. During the fuel design and evaluation process, all these design constraints are considered, additional margins for uncertainties are included, and the most limiting condition sets the maximum power rating of the fuel, and thereby the core.

Thermal conductivity of the uranium determines the temperature gradient and controls heat transfer across the uranium pellet in LWR fuel. Its magnitude is of interest since it impacts, while in turn being affected by, thermal-hydraulic and neutronic phenomena that govern reactor operation. By affecting the temperature profile, thermal conductivity influences the mechanical and thermodynamic state of the fuel pellet as well as the kinetic phenomena that govern its evolution in the irradiation environment [11,12]. Design requirements limit the maximum fuel centerline temperature to avoid excessive fission gas release under normal operating conditions and incipient melting under transient scenarios. An increase in the fuel thermal conductivity (and/or a reduction in the fuel pellet diameter) will reduce the peak fuel temperature at constant assembly power. Fuel bundle geometries in both PWRs and BWRs have evolved over the past 50 years to contain a larger number of rods with smaller diameters. As a result, significant margin in peak fuel pellet temperature exists that renders it largely irrelevant as a

[☆] The United States Government retains, and by accepting the article for publication, the publisher acknowledges that the United States Government retains, a nonexclusive, paid-up, irrevocable, worldwide license to publish or reproduce the published form of this work, or allow others to do so, for United States Government purposes.

* Corresponding author. Tel.: +1 865576 0264.

E-mail address: terrani@ornl.gov (K.A. Terrani).

potentially limiting condition under steady-state conditions. Under steady-state conditions, the thermal–hydraulic design is typically constrained by the maximum pressure drop across the fuel bundles that governs the coolant flow rate and enables the highest harvestable heat flux while maintaining hydrodynamic stability for these structures. Fuel temperature can be limiting under certain anticipated transients without scram (ATWS) scenarios where incipient melting at the centerline is not allowed by the regulator. Therefore, increased fuel conductivity has clear benefits for ATWS scenarios and would be beneficial in flattening the fuel temperature profile and further minimizing fission gas release under steady-state operating conditions, but it generally would not impact normal regulatory operating constraints for current fuel assembly geometries.

In addition to these benefits for increased fuel thermal conductivity, an important question is whether higher fuel thermal conductivity can provide enhanced accident tolerance during design basis and beyond design basis accident (DBA and BDBA) loss of coolant scenarios. The code scaling, applicability, and uncertainty (CSAU) evaluation methodology initiated in 1970s has been used to deliver a number of studies that identify and rank the most important factors contributing to core response during design basis large break (LB) LOCAs [13]. Much of the studies carried out based on CSAU methodology were specific to PWRs, and the stored energy in the fuel has been considered to be an important parameter in determining the fate of the reactor under LBLOCAs [14]. The stored energy in the fuel is inversely proportional to the fuel thermal conductivity. The purpose of this paper is to examine the effect of stored energy in the fuel (directly related to the temperature gradient across uranium pellets) on the thermal hydraulic response of the core for BWRs and PWRs separately during a design basis LBLOCA scenario. As will be discussed in detail in later sections, a LBLOCA is considered the case under which core response is most sensitive to the initial stored energy in the fuel. Therefore, understanding core response sensitivity to the magnitude of fuel thermal conductivity under a LBLOCA will enable one to conjecture about the sensitivity of core response to this parameter during other possible scenarios extending beyond the design basis limit. Understanding these effects will in turn inform and guide researchers to focus on productive areas of research and development for accident-tolerant fuel concepts and enhancement of core safety margins.

The magnitude and the rate of dissipation of stored energy from the fuel are initially examined using a simple numerical model that solves the one-dimensional transient heat conduction equation in a PWR fuel rod subjected to a scram without any loss of cooling. Subsequently the effect of enhanced fuel thermal conductivity on fuel temperature evolution during a LBLOCA is examined for BWRs and PWRs separately. The TRACE code [15] is utilized to perform full-core simulations with fuel thermal conductivity at nominal and arbitrarily increased values. In this manner, the effect of thermal conductivity on the fuel temperature rise during this transient is analyzed and compared within the two reactor platforms.

2. Methodology

2.1. One-dimensional transient heat conduction for a PWR rod subjected to scram

The evolution in temperature distribution across the radial position (r) of a cylindrical fuel pellet can be determined from the transient heat conduction equation:

$$\frac{\partial}{\partial t}(\rho C_p T) = \frac{1}{r} \frac{\partial}{\partial r} \left(r k \frac{\partial T}{\partial r} \right) + Q, \quad (1)$$

where ρ , C_p , and k are the density, heat capacity, and thermal conductivity of the fuel, respectively. Note that the temperature dependence of all these parameters needs to be accounted for by taking into account their radial dependence. Thermal conductivity of uranium dioxide fuel has been examined extensively [16] through a series of out-of-pile [17–20] and in-pile [21–24] experiments to determine its dependence on fuel temperature and burnup. For purposes of this analysis, the correlation for thermal conductivity from Wiesenack et al. [23] is used:

$$k = \frac{1}{0.1148 + 0.0035\beta + (2.475 \times 10^{-4} - 8 \times 10^{-4}\beta)T} \text{ [W/m K]}, \quad (2)$$

where β is the fuel burnup in MWd/kg-UO₂ and T is temperature in °C (Fig. 1). Note that the thermal conductivity values determined from Eq. (2) are based on in-pile measurements that capture the overall effect of various processes affecting thermal transport across the fuel pellet. These processes include thermal conductivity degradation due to pellet cracking, soluble fission products, and gas-filled porosity as well as thermal conductivity enhancement due to metallic second phase and high-burnup structures (HBS) in the outer rim region of the fuel [25]. The temperature-dependent heat capacity for the UO₂ fuel was extracted from MATPRO correlations [26]. Q is the volumetric heat generation rate in the fuel pellet. Its value is constant as a function of time under steady-state operation and drops exponentially upon initiation of scram. The decay heat generation term was approximated using [27]

$$Q_{\text{decay}} = 9.5 \times 10^{-2} Q_0(t)^{-0.26}, \quad (3)$$

where t designates the time in s after scram and Q_0 is the volumetric heat generation rate under normal operating conditions. Note the implicit assumption here that Q is constant within the fuel pellet for the sake of simplicity (i.e., radial and azimuthal variations in fission density are ignored). To solve Eq. (1), boundary conditions at the fuel pellet centerline and surface need to be specified. The former is essentially a zero flux Neumann boundary condition, while the latter corresponds to the heat flux away from the pellet. The circumferentially integrated heat flux away from the pellet is equal to the circumferentially integrated heat flux into the gap, cladding, and coolant. The temperature drop from the fuel pellet surface to the bulk coolant flowing between the fuel rods can be estimated using

$$T_{R_f} - T_{\infty} = \frac{1}{h} J_{R_f}, \quad (4)$$

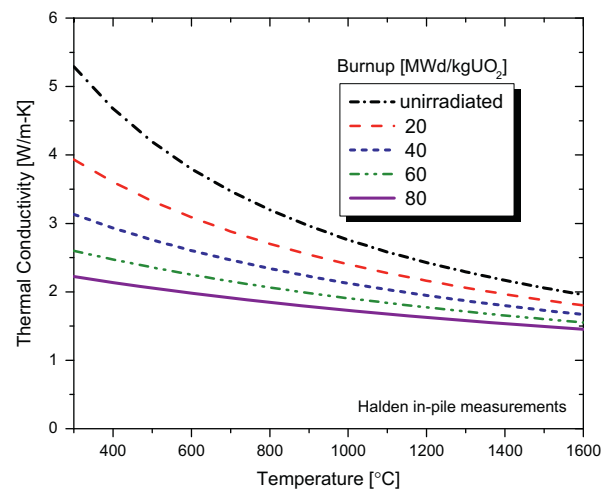


Fig. 1. Burnup- and temperature-dependent thermal conductivity of uranium [23].

where subscripts R_f and ∞ designate fuel pellet radius and bulk coolant, respectively, J is the heat flux, and h is the effective conduction term from the pellet surface to the bulk coolant. Given the nature of the heat transport processes in the cylindrical coordinates, h is defined as the inverse of the sum of thermal resistances:

$$\frac{1}{h} = R_f \frac{\ln\left(1 + \frac{\delta_{gap}}{R_f}\right)}{k_{gap}} + (R_f + \delta_{gap}) \frac{\ln\left(1 + \frac{\delta_{clad}}{R_f + \delta_{gap}}\right)}{k_{clad}} + \frac{1}{h_{hyd}}, \quad (5)$$

where δ designates the thickness of the gap or cladding and h_{hyd} is the heat transfer coefficient from the cladding to the coolant. The terms on the right-hand side of Eq. (5) represent the thermal resistance across the fuel-pellet gap, cladding, and hydraulic boundary layer, respectively. h_{hyd} varies significantly as the temperature gradient (heat flux) from the cladding surface to the coolant varies and as flow conditions alter [3]. In this particular analysis, the inherent assumption is that no loss of fuel rod cooling takes place after scram is initiated and the pumps continue to maintain coolant flow across the fuel bundles. Although the heat transfer mode and therefore heat transfer coefficient change as the rod power decreases, a constant value for h_{hyd} is assumed in this analysis for the sake of simplicity. This scenario where forced convective cooling by flowing water remains in place differs from a LBLOCA scenario where h_{hyd} can drop by up to four orders of magnitude [28] due to a significant loss in core flow.

The constant physical properties and linear heat rate (LHR) of the rod along with geometrical parameters used for this analysis are listed in Table 1. The rod geometry for this analysis was designated to be that of PWR 17 × 17 fuel bundle [29]. A constant value of pellet-cladding gap has been maintained during the fuel burnup that artificially increases fuel temperature at higher burnups. Under actual in-pile conditions, cladding creepdown and pellet swelling reduce the gap where complete closure occurs at ~40MWd/kgU. Given the boundary conditions defined earlier, Eq. (1) is discretized and solved using a semi-implicit Crank–Nicolson scheme [30]. Radially variable thermal conductivity and heat capacity are updated at each time step to incorporate the effect of an evolving temperature profile. The spatial and temporal mesh was set at 2.7 μm and 3 ms intervals, respectively. The details of the discretization and solution methodology are similar to the approach taken in a prior publication and are therefore omitted here [31].

2.2. LBLOCA analysis using TRACE

TRAC/RELAP Advanced Computational Engine (TRACE) code is the latest reactor systems code developed by the U.S. Nuclear Regulatory Commission for performing best-estimate analyses of LOCAs, operational transients, and other accident scenarios in PWRs and BWRs [15]. In this study, it was used to perform sensitivity analysis of core response to fuel thermal conductivity during a LBLOCA in typical BWR and PWR designs. Basic details of the analysis input for BWR and PWR cases are described below, while

useful background information on basic reactor design and safety systems can be found in U.S. NRC's reactor concepts manuals.

2.2.1. BWR analysis input

A typical General Electric BWR4 plant with a Mark-I containment loaded with GE14 bundles [29] (Table 2) was examined during this analysis. The TRACE model includes all of the major flow paths and system components to perform large and small break LOCA simulations. Both recirculation loops are modeled explicitly. The reactor pressure vessel (RPV) is modeled using the TRACE VESSEL component with axial levels, radial rings, and azimuthal sectors representing the downcomer, lower plenum, core, upper plenum, and upper head regions. The GE14 fuel bundles are modeled using TRACE CHAN components. Also modeled are the drywell and suppression chamber components with a CONTAN component. The steam lines are modeled out to the turbine control valve. The feedwater system is modeled with a FILL component set up to control the feedwater flow rate to maintain the desired downcomer level in the RPV. The emergency core cooling systems (ECCS) are also modeled using FILL components and include the high- and low-pressure coolant injection systems (HPCI and LPCI) as well as low-pressure core sprays (LPCS). More detailed description of the analysis framework is provided elsewhere [32].

A double-ended guillotine recirculation suction line break (0.36 m²) is assumed to mark the onset of transient and time zero, as shown in Fig. 2. The two recirculation pumps are tripped and the reactor is scrammed at the same time. Only two LPCIs are assumed to be available. High-pressure core sprays (HPCS) and LPCS, as well as the automatic depressurization system (ADS), are all assumed to be unavailable. LPCI injection is actuated as designed starting at 91 s after the break.

2.2.2. PWR analysis input

A typical 4-loop Westinghouse design PWR loaded with Westinghouse 17 × 17 fuel assemblies (Table 2) was employed for this study. The PWR TRACE input file models all of the major flow paths in the system needed to perform large and small break LOCA and SGTR simulations. All four recirculation loops are modeled explicitly. The reactor pressure vessel is modeled using the TRACE VESSEL component with 26 axial levels, four radial rings and eight azimuthal sectors. The core region is located in Level-5 to Level-18 of Ring-1 and Ring-2, and all levels have the same axial lengths of 0.261257 m. Ring 3 includes the barrel-baffle bypass region, while Ring 4 includes the downcomer. Among all four reactor coolant system (RCS) primary recirculation loops, the RCS Loop 1 model represents the broken primary loop (Fig. 3). As such, it has modeling features that allow simulating the side breaks in the loop cold leg. The RCS Loop 2 model includes the model of the reactor pressurizer vessel and the surge line that connects the pressurizer to the hot leg of this loop. The TRACE models for the remaining two recirculation loops, Loop 3 and Loop 4, exhibit identical features.

A LBLOCA was simulated using the TRACE steady-state input model of the PWR. The LBLOCA was a double-ended guillotine break in cold leg 1. For this event, it was assumed that all ECCS

Table 1

Physical properties and geometry used for one-dimensional transient heat conduction analysis.

Parameter	Value	Units
ρ_{UO_2}	10.47	g/cm ³
k_{gap}	0.5	W/m K
k_{clad}	18	W/m K
LHR	200	W/cm
h_{hyd}	2	W/cm ² K
R_f	4.10	mm
δ_{gap}	50	μm
δ_{clad}	575	μm

Table 2

Fuel bundle geometry used for BWR and PWR LBLOCA analysis.

Parameter	BWR	PWR	Units
R_f	4.38	4.10	mm
δ_{gap}	89	83	μm
δ_{clad}	660	572	μm
Rod pitch	12.95	12.60	mm
Active fuel length	3.81	3.66	m
Active fuel length (part-length rods)	2.44	N/A	m

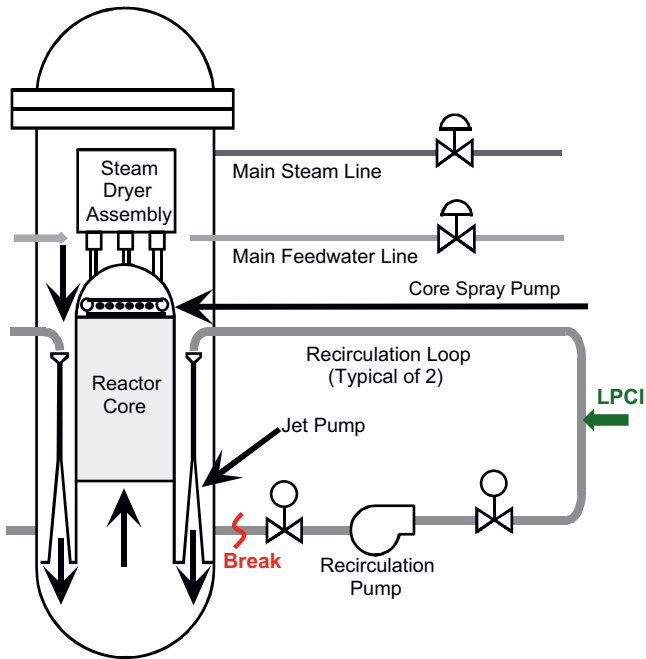


Fig. 2. Schematic drawing of BWR core subjected to LBLOCA (modified image after U.S. NRC).

systems were available in the TRACE simulation. The large break is assumed to be located between the ECCS injection line and the RPV. A break area of 80% of the full cold leg pipe flow area is used. Double-ended cold leg break, reactor coolant pump (RCP) trip, and reactor/turbine trip are all assumed to initiate at time 0. The safety injection actuation signal (SIAS) is delivered 6 s later, and accumulator injection (intact loops) begins after 13 s. ECCS pumped flow is initiated at 36 s.

The results reported in the next section are for the hottest rod in the core with an axial LHR distribution as shown in Fig. 4 for the BWR and PWR platforms separately. Taking into account all the pertinent conduction, convection, and radiation heat transfer phenomena, the code reports global maxima for fuel centerline and cladding temperatures in the results section.

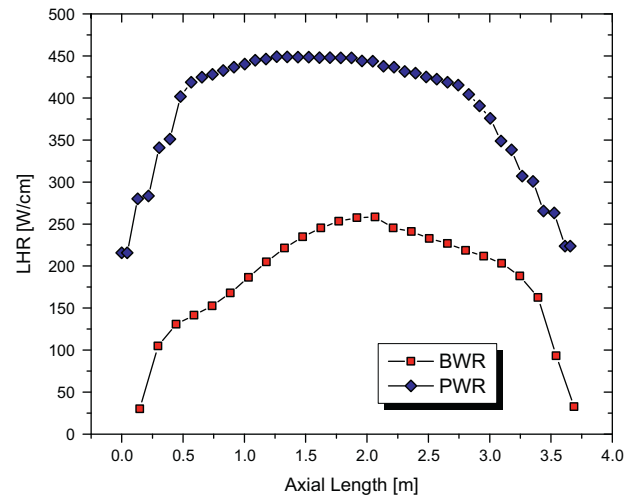


Fig. 4. Linear power profile for the hottest rod under normal operating conditions in the simulated BWR and PWR cores prior to LBLOCA simulation.

3. Results

3.1. Magnitude and rate of dissipation of stored energy in a PWR pin after scram

Given the methodology outlined in Section 2.1, the evolving temperature profile in a PWR fuel pellet before and after core scram is shown in Fig. 5 for two different fuel burnups. In this analysis, no loss of cooling capability takes place and the bulk coolant temperature is kept constant at 300 °C along with the cladding-to-coolant heat transfer coefficient at 2 W/cm² K for simplicity. As shown in the figure, the temperature profile in the fuel rapidly flattens and the stored heat is almost completely dissipated into the coolant after 15 s. The small temperature gradient remaining across the fuel pellet after an extended period is due to the small amount of decay heat that is being generated in the fuel volume given by Eq. (3).

The magnitude of stored energy in the fuel can be estimated as the difference in heat content of the fuel under normal operating conditions compared to long periods after initiation of scram.

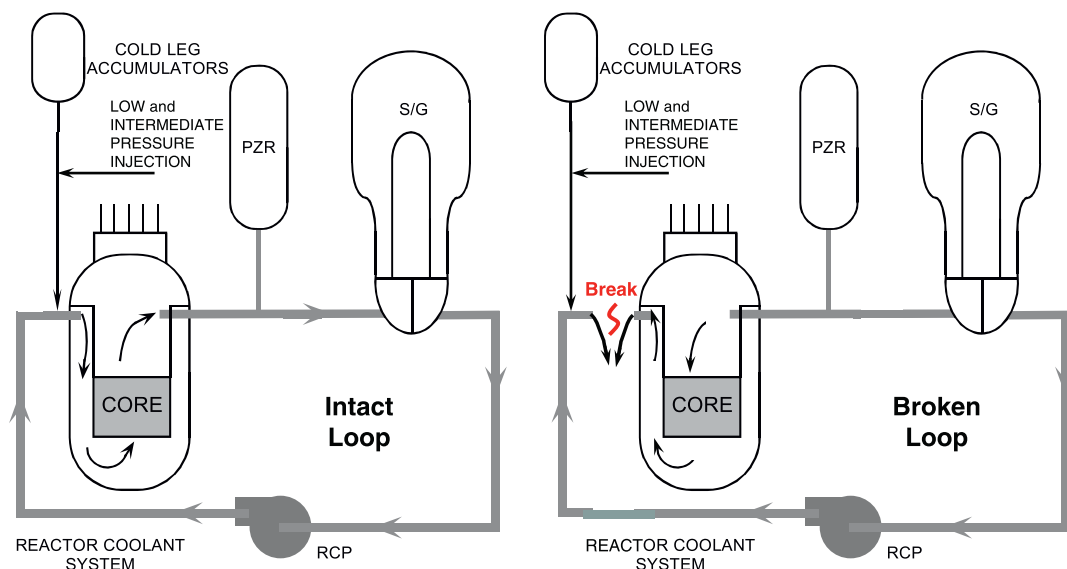


Fig. 3. Schematic drawings of intact and broken loops in a PWR core subjected to LBLOCA (modified image after U.S. NRC).

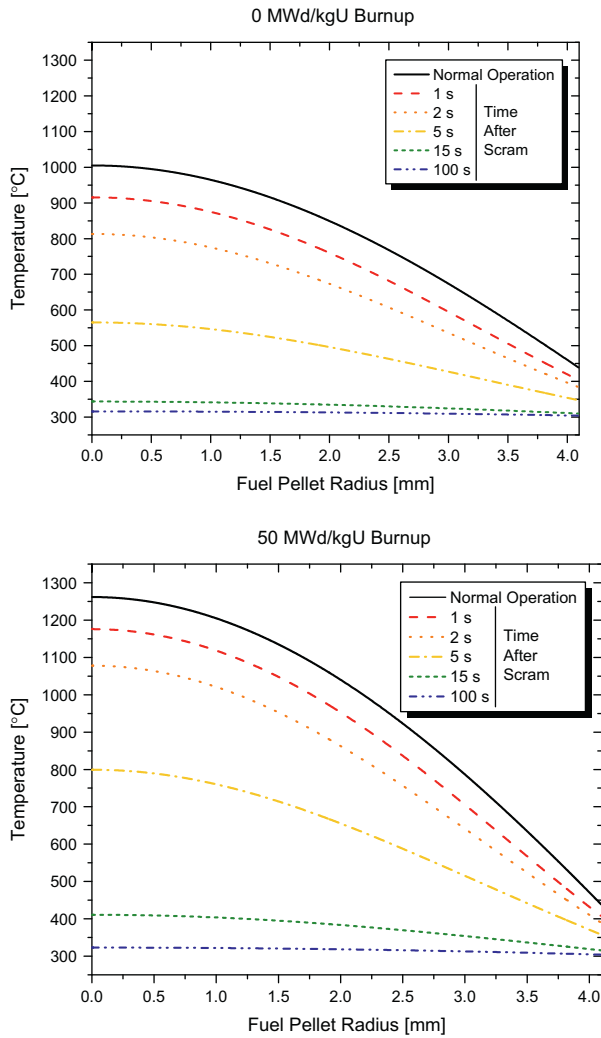


Fig. 5. Temperature profile across the fuel pellet as a function of time after initiation of scram for various burnups.

The linear heat content in the cylindrical pellet, H , can be determined as follows:

$$H = 2\pi \int_0^R r \rho C_p T dr - H_{ref}, \quad (6)$$

where H_{ref} is the linear heat content at any reference temperature. Radial distribution of temperature and heat capacity under any operating condition (e.g., steady-state operation, transient conditions after scram initiation) are input into Eq. (6), which is then integrated numerically. The extent of stored energy in the fuel that is released into the coolant is estimated as the difference in heat content under normal operating conditions and 100 s after scram is initiated. Fig. 6 shows the magnitude of stored energy in the fuel as a function of the fuel burnup. These values are subject to the rod operating power and consistent with similar values reported elsewhere [33]. Since the thermal conductivity in the fuel decreases as burnup increases (Fig. 1) and therefore the fuel interior temperature increases, the magnitude of stored energy in the fuel increases with burnup for a constant linear power. Using the estimate for decay heat generation in the fuel provided by Eq. (3), one can perform time integration to estimate the cumulative heat generation due to decay of radioisotopes present in the fuel. Fig. 7 shows the magnitude of this parameter as a function of time after scram. By comparing Figs. 6 and 7, it becomes apparent that the linear heat

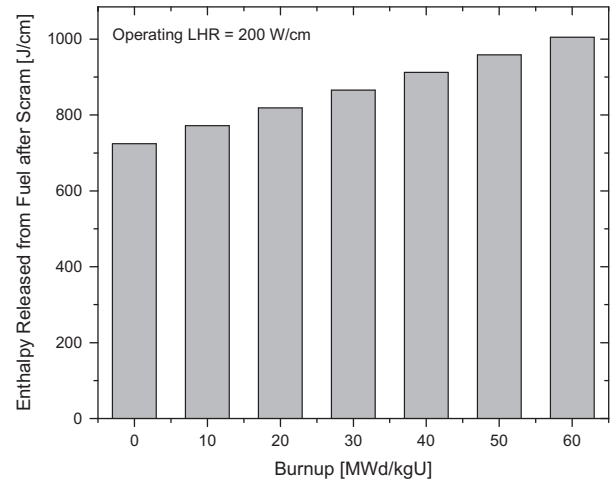


Fig. 6. Magnitude of stored energy in the fuel released after scram is initiated as a function of burnup.

content released from the stored energy in fuel is on the order of the cumulative decay heat generated after 100 s. At longer times the former becomes a negligible fraction of heat deposited in the core.

3.2. LBLOCA simulation of nominal and enhanced thermal conductivity fuels

3.2.1. BWR LBLOCA

Fig. 8 shows the fuel centerline and peak cladding temperature (FCT and PCT) for the hottest rod in the GE BWR-4 (Mark I containment) LBLOCA simulation with nominal and enhanced fuel thermal conductivity. The radial fuel temperature profile flattens rapidly for all three cases upon initiation of the scram. After only 15 s past the scram the FCT and PCT are essentially identical for all cases with only a negligible difference between the fuels with nominal and enhanced thermal conductivity. At any time after the onset of the break/scram, the PCT for fuel with nominal thermal conductivity is nearly identical with that of the fuel with enhanced (up to 500%) thermal conductivity. The LPCI that is actuated at 91 s after scram eventually results in core quench at about 170 s with only minor differences due to fuel thermal conductivity. Note that while the increase in fuel thermal conductivity significantly alters the temperature profile across the pellet during normal operation, it

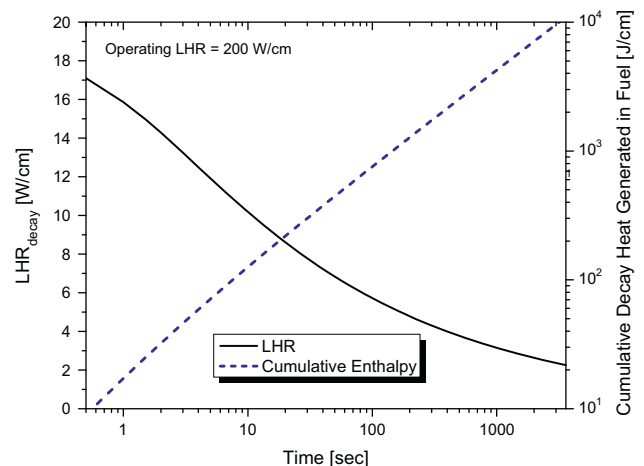


Fig. 7. Rate and cumulative extent of decay heat generated in the fuel after scram.

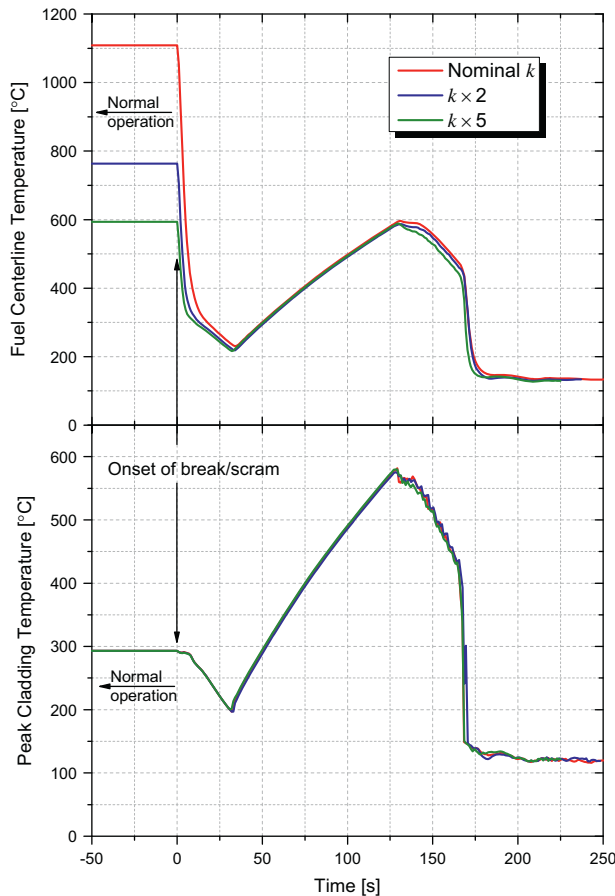


Fig. 8. FCT and PCT evolution of the hottest rod during a simulated LBLOCA in typical BWR-4 loaded with fuel at nominal and enhanced thermal conductivity.

has no discernible effect on the fuel and cladding temperature under this LBLOCA scenario.

The explanation for lack of any notable difference in PCT evolution among these three cases is inherent to the thermal-hydraulics of the BWR. Although pumps trip at the onset of the break, the pumping action coasts down slowly and the flow down the jet pumps continues to inject water upwards into the core. The water moves upward within the fuel bundles, undergoes phase change as under nominal operating conditions, and removes the heat generated inside the fuel rods. As shown in the figure, this cooling action continues for roughly 30 s where the stored energy in the fuel is removed (FCT for the various cases is nearly identical) and the PCT for all cases drops. Once the stored energy from the fuel is removed, the rod responses are identical among all the examined cases since the decay heat solely depends on the power history.

3.2.2. PWR LBLOCA

Fig. 9 shows the FCT and PCT for the hottest rod in the 4-loop PWR during the LBLOCA simulation with nominal and enhanced fuel thermal conductivity. A notable difference in the magnitude of FCT and PCT during the course accident is apparent for various cases. An increase in fuel thermal conductivity by 200% and 500% reduces PCT by 56 °C and 92 °C, respectively; also, the increase in fuel thermal conductivity results in faster quench times [for nominal, 200% and 500%: the quench times are 213 s, 194 s, and 177 s, respectively (from Fig. 9)]. The magnitude of this change normalized against the thermal resistance of the fuel (directly proportional to the stored energy) is consistent with earlier analyses focused on parameter uncertainty [33]. This contrast in fuel

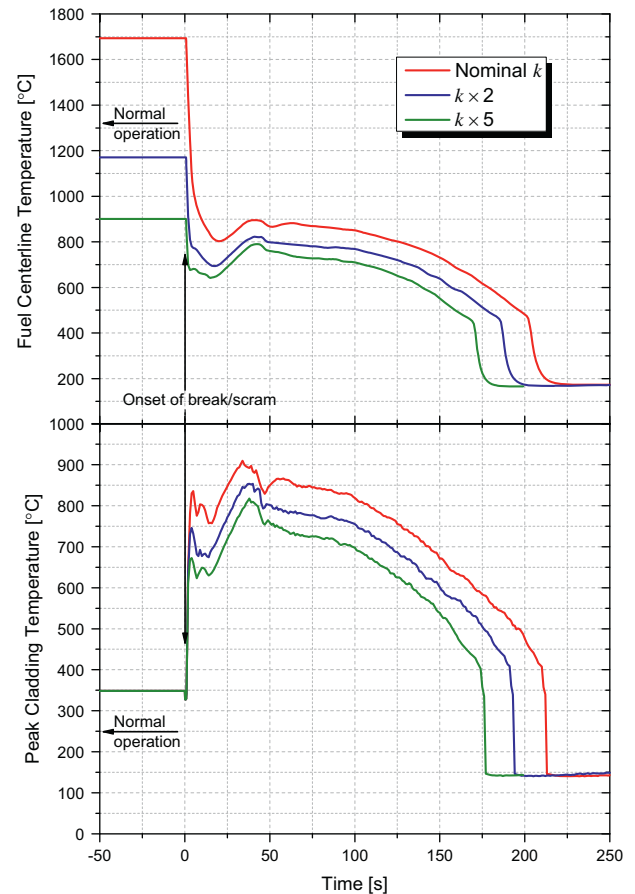


Fig. 9. FCT and PCT evolution of the hottest rod during a simulated LBLOCA in typical 4-loop PWR loaded with fuel at nominal and enhanced thermal conductivity.

temperature response between BWR and PWR fuel rods can be explained by the disparity in the thermal-hydraulic design and response of the core during the LBLOCA and is discussed in the next section.

In the PWR, upon the break opening in the cold leg, the large pressure difference between the RCS and the containment forces coolant water rapidly out of the broken cold leg. Accordingly, almost no cooling of the rod after scram that could result in removal of stored energy is experienced. Initially, subcooled water is expelled out of the break given the high RCS pressure. As the RCS pressure quickly falls down to the saturation pressure, a two-phase mixture of steam and water is expelled out due to coolant flashing. The break changes from subcooled to saturated critical flow, and the break mass flow rate decreases rapidly. The mass flow from the vessel side of the break is much larger than from the pump side because of the large flow resistances of the RCP and the steam generator tubes. The reactor trip is supposed to occur on a low pressurizer pressure signal with a delay. It is also assumed that a loss-of-offsite power occurs coincident with the cold leg break. This results in a RCP trip at 0.0 s.

Due to the loss of inventory and flashing in the core, the core is uncovered. As the RCS pressure falls below that of the nitrogen gas in the accumulators, the check valves open and accumulator flow begins at 13.0 s. Each of the four accumulators inject into a different cold leg. This results in the flow from one accumulator going directly into containment through the broken cold leg (Fig. 3). The large flow from the accumulators begins to refill the RPV, and just after 33 s, the core reflood begins. The maximum of the peak cladding temperature occurs at around 34 s and decreases

as the core is refilled, and decay heat decreases. The accumulators empty by 54 s. A safety injection actuation signal is generated at 6.0 s. After a 30 s startup delay time, the centrifugal charging pumps, safety injection pumps, and the residual heat removal pumps begin the flow into the RCS. Similar to the accumulators, this flow is injected into the cold legs. This results in a single train of the ECCS flowing directly into containment through the broken cold leg.

4. Discussion

4.1. PCT sensitivity to fuel thermal conductivity during BWR and PWR LBLOCA

While almost no effect from fuel thermal conductivity on PCT evolution is observed for the BWR rod, a notable one is shown to exist for the PWR fuel pins. This disparity is explained by the thermal-hydraulics aspects of the two different cores and their ability to remove the stored energy in the fuel during the transient. In the BWR core, a break in the suction line does not alter the flow direction. Though both of the recirculation pumps are tripped, they continue to inject water down the jet pumps during coast down and facilitate upward coolant flow into the core. The water, as it is the case under nominal operating conditions, flashes into steam and removes heat as it climbs upward within the fuel bundles. The cooling process continues for ~ 30 s, which is long enough when compared to characteristic heat transport time across the radial direction of the fuel (~ 15 s as discussed in Section 3.1). Accordingly, the magnitude of the remaining stored energy in the fuel is negligible and no discernible effect on PCT evolution is observed during later stages of the transient.

For the PWR, the break in the cold leg results in rapid coolant discharge from the core (Fig. 3). The coolant entering from the cold leg in the three remaining intact loops is unable to continue flowing upward into the core during the depressurization process. Accordingly, no significant heat removal from the fuel takes place and the initial stored energy in the pellet simply redistributes across the pellet and cladding (with, as a result, increased PCT), as opposed to being removed. The larger magnitude of stored energy in the fuel with lower thermal conductivity results in a larger PCT shortly after the onset of the break. This larger temperature persists across the core until it is quenched. The drop in PCT during this analysis by 56°C upon doubling the fuel thermal conductivity provides some cladding burst margin across the core and results in a lower burst fraction [34].

4.2. Effect of fuel thermal conductivity on core response during longer term accidents

Once the stored energy in the fuel has been removed, no significant effect of fuel thermal conductivity on core response during LOCAs is observed. Therefore, the accident scenarios most sensitive to the magnitude of fuel thermal conductivity are the ones in which the cooling capability is significantly reduced or lost in short time periods below what is the characteristic time for heat transport across fuel pellets and discharge of stored energy. As was pointed out during the simplified analysis in Section 3.1, the characteristic time for stored energy release from the pin is on the order of 15 s. Therefore if adequate cooling (passive or active) is maintained in the core for periods extending beyond this limit, the magnitude of stored energy can be assumed to be negligible.

This logic can be extended to predict the sensitivity of core response to fuel thermal conductivity during station blackout scenarios (SBOs). For a short-term SBO where complete loss of power takes place at the onset of the accident, the magnitude of stored

energy and the effect of fuel thermal conductivity can be significant (even though coast down of the pumps in the intact loops is expected to provide some cooling). However, for the longer term SBO scenarios where some level of core cooling was maintained for a period prior to complete loss of power, the stored energy can be ignored as a contributing factor to accident progression at later stages. For instance, in Fukushima Daiichi units 1–3 (BWRs instead of PWRs), all units experienced automatic scram once the earthquake was detected and experienced active cooling for roughly an hour prior to the SBO that ensued [35]. Therefore, the stored energy in the fuel dissipated soon after the reactor scram and did not contribute to the core degradation during the ensuing SBO scenario.

4.3. Important fuel-related parameters for core performance under LOCAs

Given that the characteristic time for heat transport across the fuel pellet is ~ 15 s, one expects the temperature profile across the pellet to flatten at times extending beyond this limit. This is shown in Fig. 10, where after 15 s past the scram only a small temperature gradient is observed across the fuel pins (due to the small magnitude of decay heat generation). Given the negligible temperature gradient across the fuel and the cladding, the first term on the right-hand side of Eq. (1) can be ignored. Therefore, the rate of temperature rise becomes directly proportional to decay heat generation rate and inversely proportional to the heat capacity of the fuel constituents. As discussed earlier, the decay heat generation rate is directly proportional to operating LHR [Eq. (3)]; burnup and power history effects are deemed negligible for the purposes of this discussion. Accordingly, limiting radial and axial core power peaking by optimized neutronic design can limit axial peaking of PCT and burst frequency. The heat capacity of the fuel and the cladding is another important parameter. By transitioning to cladding materials that exhibit higher heat capacity than zirconium alloys (e.g., iron alloys), the rate of temperature increase in the fuel can be reduced [32]. If core cooling capability is lost in certain BDBA scenarios, the core temperature continues to increase and extends beyond what is shown in Fig. 6. Once temperatures beyond $\sim 1200^\circ\text{C}$ are reached, the heat generation in the core as a result of zirconium cladding oxidation becomes significant (often exceeding the decay heat) and further exacerbates core degradation processes. Under these circumstances, reduction in the oxidation

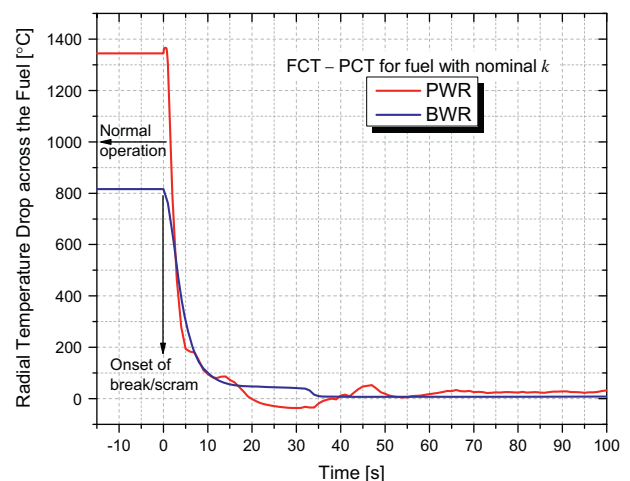


Fig. 10. Evolution of temperature gradient across the fuel pellet for the BWR and PWR pin during LBLOCA.

kinetics of cladding and core constituents by steam can play a significant role in enhancing safety margins [36].

5. Conclusions

The characteristic time for heat transport across UO_2 pellets in LWR fuel rods is on the order of 15 s. During an accident, if cooling capability is maintained after core shutdown, the stored energy in the fuel (inversely proportional to fuel thermal conductivity) discharges in a similar time scale and will not affect the later progression of cladding and fuel temperatures. Also, the magnitude of stored energy in the fuel is comparable to the cumulative decay heat produced in the core after a few minutes. Therefore, for longer term accidents (e.g., where significant loss of cooling occurs after a few minutes past the onset of scram), decay heat generation dominates heat deposition inside the core.

To fully grasp the sensitivity of core response to fuel thermal conductivity, the details of the core thermal hydraulic design need to be considered. Simulations of a BWR and a PWR subjected to a LBLOCA scenario were carried out, and a different response was recorded for each case. For the BWR, an increase in fuel thermal conductivity up to 500% has essentially no effect on the fuel rod heat-up rate following coolant loss. This is attributed to the nature of coolant flow inside the vessel that facilitates continued cooling after reactor scram and pump trip, and in turn removes the stored energy in the fuel. For the PWR, core flow is lost soon after RCPs are tripped during the early blowdown stage of the transient, and the stored energy is not discharged from the fuel. Therefore, an increase in fuel thermal conductivity will have a modest effect in reducing the PCT as the accident progresses.

Acknowledgments

Useful input from Steven Zinkle, Lance Snead, and Kevin Robb at ORNL are gratefully acknowledged. The work presented in this paper was supported by the Advanced Fuels Campaign of the Fuel Cycle R&D program in the Office of Nuclear Energy, US Department of Energy.

References

- [1] D. Olander, *Journal of Nuclear Materials* 389 (2009) 1–22.
- [2] S. Zinkle, G. Was, *Acta Materialia* 61 (2013) 735–758.
- [3] N.E. Todreas, M.S. Kazimi, *Nuclear Systems I: Thermal Hydraulic Fundamentals*, Taylor and Francis, 1993.
- [4] C. Shuffler, J. Trant, J. Malen, N. Todreas, *Nuclear Engineering and Design* 239 (2009) 1442–1460.
- [5] P. Ferroni, C. Handwerk, N. Todreas, *Nuclear Engineering and Design* 239 (2009) 1544–1559.
- [6] Nuclear Fuel Safety Criteria Technical Review, NEA Report No. 7072, third ed., Nuclear Energy Agency, OECD, Paris 2012.
- [7] Standard Review Plan for the Review of Safety Analysis Reports for Nuclear Power Plants, LWR Edition, NUREG-0800, Section 4.2 Fuel System Design, Rev. 3, US Nuclear Regulatory Commission, Office of Nuclear Reactor Regulation, March 2007.
- [8] 10 Code of Federal Regulation 50.46.
- [9] Draft Regulatory Guide DG-1263, US Nuclear Regulatory Commission Office of Nuclear Regulatory Research, 2012.
- [10] J. Alvis, W. Liu, R. Montgomery, *Fuel Reliability Program: Proposed RIA Acceptance Criteria*, Report No. 1021036, Electric Power Research Institute, Palo Alto, CA, 2010.
- [11] D.R. Olander, *Fundamental Aspects of Nuclear Reactor Fuel Elements*, TID-26711-P1, Technical Information Center, Springfield, VA, 1976.
- [12] P.G. Lucuta, H. Matzke, I.J. Hastings, *Journal of Nuclear Materials* 232 (1996) 166–180.
- [13] B. Boyack, G. Lellouche, *Nuclear Engineering and Design* 119 (1990) 1–15.
- [14] G.E. Wilson, *Nuclear Engineering and Design* 119 (1990) 17–31.
- [15] TRACE: The TRAC/RELAP Advanced Computational Engine, US Nuclear Regulatory Commission, 2012.
- [16] D. Staicu, 2.17 – Thermal Properties of Irradiated UO_2 and MOX, in: *Comprehensive Nuclear Materials*, J.M.K. Editor-in-Chief: Rudy, Ed., ed Oxford, Elsevier, 2012, pp. 439–464.
- [17] S. Yagnik, Thermal conductivity recovery phenomenon in Irradiated UO_2 and (U, Gd) O_2 , presented at the ANS Topical Meeting on LWR Fuel Performance, Park City, Utah, 2000.
- [18] M. Amaya, M. Hirai, H. Sakurai, K. Ito, M. Sasaki, T. Nomata, et al., *Journal of Nuclear Materials* 300 (2002) 57–64.
- [19] C. Ronchi, M. Sheindlin, D. Staicu, M. Kinoshita, *Journal of Nuclear Materials* 327 (2004) 58–76.
- [20] C. Walker, D. Staicu, M. Sheindlin, D. Papaioannou, W. Goll, F. Sontheimer, *Journal of Nuclear Materials* 350 (2006) 19–39.
- [21] I. Cohen, B. Lustman, J.D. Eichenberg, *Journal of Nuclear Materials* 3 (1961) 331–353.
- [22] P. Betten, *Transactions of the American Nuclear Society* 50 (1985) 239–240.
- [23] W. Wiesenack, M. Vankeerberghen, R. Thankappan, Assessment of UO_2 conductivity degradation based on in-pile temperature data, HWR-469, Institut für energietechnik, OECD Halden Reactor Project, 1996.
- [24] Y. Rashid, R. Dunham, R. Montgomery, *Fuel Analysis and Licensing Code: FALCON MOD01: Volume 1: Theoretical and Numerical Bases*, EPRI 1011307, Electric Power Research Institute, 2004.
- [25] V.V. Rondinella, T. Wiss, *Materials Today* 13 (2010) 24–32.
- [26] SCDAP team, SCDAP/RELAP5/MOD 3.2 Code Manual, Volume IV: MATPRO – A Library of Materials Properties for Light-Water-Reactor Accident Analysis, NUREG/CR-6150, vol. 4, October 1997.
- [27] M.M. El-Wakil, *Nuclear Heat Transport*, American Nuclear Society, 1971.
- [28] Fuel Cladding Failure Criteria, EUR 19256 EN, European Nuclear Commission, 1999.
- [29] Fuel design data, *Nuclear Engineering International*, vol. 49, September, pp. 26–35, 2004.
- [30] J. Crank, P. Nicolson, A practical method for numerical evaluation of solutions of partial differential equations of the heat-conduction type, in: *Mathematical Proceedings of the Cambridge Philosophical Society*, 1947, pp. 50–67.
- [31] K.A. Terrani, J.E. Seifried, D.R. Olander, *Journal of Nuclear Materials* 392 (2009) 192–199.
- [32] L.J. Ott, K.R. Robb, D. Wang, Preliminary assessment of ATFs on LWR during normal operation and under DB and BDB accident conditions, *Journal of Nuclear Materials*, 448 (2014) 520–533, <http://dx.doi.org/10.1016/j.jnucmat.2013.09.052>.
- [33] W. Wulff, *Nuclear Engineering and Design* 119 (1990) 33–65.
- [34] D. Powers, R. Meyer, *Cladding Swelling and Rupture Models for LOCA Analysis*, NUREG-0630, US Nuclear Regulatory Commission, 1980.
- [35] R. Gauntt, D. Kalinich, J. Cardoni, J. Phillips, A. Goldmann, S. Pickering, et al., Fukushima Daiichi Accident Study (Status as of April 2012), SAND2012-6173, Sandia National Laboratory, July 2012.
- [36] K.A. Terrani, S.J. Zinkle, L.L. Snead, *Journal of Nuclear Materials* 448 (2014) 420–435. <http://dx.doi.org/10.1016/j.jnucmat.2013.06.041>.