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Full core LOCA safety analysis for a PWR containing high burnup fuel☆

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1. Introduction

For economic reasons, the US nuclear industry is renewing efforts to build a technical basis to extend peak rod average burnup limits above the current regulatory burnup limit of 62 GWd/MTU (Burns et al., 2020). The primary driver of these efforts is to economically increase pressurized water reactor (PWR) cycle lengths to 24 months, reduce the number of fresh fuel assemblies, increase time online, thereby reducing number of outages and their associated cost, higher fuel utilization, and possibly reduce core design constraints. In order for US nuclear utilities to leverage these economic efficiencies, the US Nuclear Regulatory Commission (NRC) will likely require nuclear power plants (NPPs) to analyze a number of potential operational occurrences and their potential consequences with each new core design prior to resuming normal operation (Burns et al., 2020). Potential operational occurrences can be divided into three primary regimes: (1) normal operation, (2) anticipated operation occurrences (AOOs), and (3) design basis accidents (DBAs). Normal plant operation is an operating regime in which the plant operates within specified operational limits until the end of the cycle, whereas AOOs are events that result in the NPP deviating outside the normal operating regime. A key attribute of an AOO is that the occurrence should be expected. However, by definition, the occurrence of an AOO does not result in significant impact to critical safety functions. The last potential operational occurrence is a DBA. From the fuel performance point-of-view, DBAs can be subdivided into two bounding categories: (1) loss of coolant accidents (LOCAs), and (2) reactivity insertion accidents (RIAs). Unlike AOOs, DBAs may result in fuel rod failure. The NRC imposes fundamental acceptance criteria to minimize radiological consequences to the public and onsite staff. Furthermore, safety criteria are typically linked to the fulfillment of other acceptance criteria related to reactor safety equipment designed to mitigate DBAs.

A LOCA is one of the most important postulated accident scenarios for light-water reactors (LWRs). A LOCA occurs when the primary coolant system suffers a failure resulting in the loss of reactor coolant. The most severe (and least probable) accident initiating event is a double-ended guillotine break between the reactor vessel and the main circulation pump. An example of a PWR fuel rod response during a double-ended guillotine break is illustrated in Fig. 1 (Erbacher et al., 1987). Following rupture of the cold legs, the coolant flashes to steam and blows down through the ruptured pipes. Fission product decay heat continues to heat the fuel and reactor internals in the wake of coolant loss. This further supports the need to establish long-term cooling to mitigate such accident consequences. Reactor depressurization is completed within ~ 20–30 s into the transient. Once depressurized, the emergency core cooling system (ECCS) begins pumping water into the reactor primary vessel (RPV). Initially, the ECCS is unable to provide sufficient cooling to the fuel elements, and during this time, decay heat continues to increase fuel rod temperatures (Erbacher et al., 1987) to the point at which permanent fuel damage may occur, including ballooning, burst, and ultimately, oxidation and hydriding, resulting in the loss of post quench ductility (Terrani et al., 2014). The severity of the LOCA transient is governed by a number of conditions related to the local fuel rod and assembly.

In the early- to mid-2000s, high burnup LOCA experiments performed at the Halden Boiling Water test reactor indicated that high burnup fuel, >80 GWd/MTU, was susceptible to fuel pellet fragmentation and pulverization, termed *high burnup fuel fragmentation* (HBFF), in which parts of the fuel pellet fragmented into an effective sand like consistency, with fuel particles less than 1 mm (Wiesenack, 2013; Lekkonen, 2007; Oberlander et al., 2011). Subsequently, pulverized fuel

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could relocate into the balloon region, and following burst, it could disperse from the cladding. The issue gained attention when HBFF was observed to be closer to the current US regulatory limit (62 GWd/MTU) during NRC-sponsored out-of-core integral testing at Studsvik Nuclear in early 2011 (Raynaud, 2012; Flanagan et al., 2013). The experimental results identified a burnup range between 65 and 72 GWd/MTU, at which fuel pellets begin to pulverize and become susceptible to relocation and dispersion. Examples of the post LOCA PIE documenting the cladding conditions (Fig. 2) and pulverized fuel (Fig. 3). The Studsvik test results led to NRC concerns regarding potential changes to fuel and core designs that could result in HBFF. Subsequent HBFF research has come to similar conclusions (Wiesenack, 2013; Lekkonen, 2007; Oberlander et al., 2011; Raynaud, 2012; Flanagan et al., 2013; Askeljung et al., 2012; Une et al., 2005; "NEA/CSNI/R, 2016; Kolstad, et al., 2011; Noirot et al., 2015; Bruet, et al., 1992; Capps et al., 2020a, 2021).

Analyses have been performe to evaluate integral and semi-integral rod burst experiments, as well as more complicated integral experiments performed at Halden and Studsvik (Capps et al., 2021; Govers and Verwerft, 2014; Khvostov et al., 2007; Tofino et al., 2009; Grandjean and Hache, 2004; Karlsson et al., 2018). However, these analyses were focused on evaluating the impact of various packing fractions in order to evaluate the impact of fuel relocation on peak cladding temperature (PCT). In 2018, a round robin study was conducted as a part of the Studsvik Cladding Integrity Project (SCIP) III program (Karlsson et al., 2018). The study evaluated the porosity, fission gas distribution, balloon burst criteria, and cladding strain for SCIP III LOCA experiments. Work performed at Idaho National Laboratory (INL) demonstrated BISON's capability to model fuel relocation (Gamble, 2018), and furthermore, an exhaustive overview of the LOCA capabilities in BISON was well documented (Williamson, et al., 2019). In an effort to begin evaluating high burnup effects at the core level, H. Zhang evaluated five high burnup core designs based on the South Texas Nuclear Power Plant in order to evaluate cladding burst potential at higher burnups (Zhang et al., 2019). To date, these analyses have not begun to address the NRC concerns related to fuel fragmentation and its potential to be dispersed from the fuel rod; nor have they begun to inform the industry's high burnup LOCA safety case. Therefore, a modern approach is needed to holistically evaluate the response of high burnup fuel under LOCA conditions

This project used a fully integrated approach by externally coupling

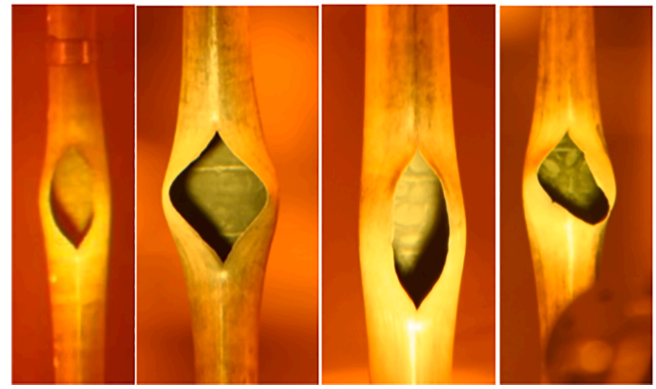


Fig. 2. Example of High Burnup Rupture Openings from integral LOCA test performed at Studsvik (Raynaud, 2012; Flanagan et al., 2013)

VERA, RELAP5-3D, and BISON to evaluate HBFF susceptibility for a 4-loop high powered Westinghouse PWR. Southern Nuclear Operating Company (SNC) developed representative core designs to support a simulation for the transition from 18- to 24-month cycles. Depletion of these core designs in VERA provided the necessary input to RELAP5-3D to evaluate thermal and pressure response of the system. Lastly, VERA and RELAP5-3D results provided BISON with the necessary boundary conditions to evaluate the evolution of high burnup fuel under steady-state and LOCA conditions in order to calculate the total mass in the core that would be susceptible to HBFF. Demonstration of these capabilities helps to inform the US nuclear industry's high burnup LOCA safety case by calculating a conservative value for mass susceptible to HBFF to support subsequent accident consequence analysis. Furthermore, this evaluation can serve as a benchmark problem for fuel suppliers, utilities, and research organizations to which they can compare the results of their codes. The full core LOCA results can be used to develop targeted separate effects testing designed to evaluate HBFF relocation and dispersal. Separate effects test results can be used to validate and refine the full core high burnup LOCA results and to support justification for large-scale integral high burnup LOCA testing at INL's Transient Reactor Test Facility (TREAT).

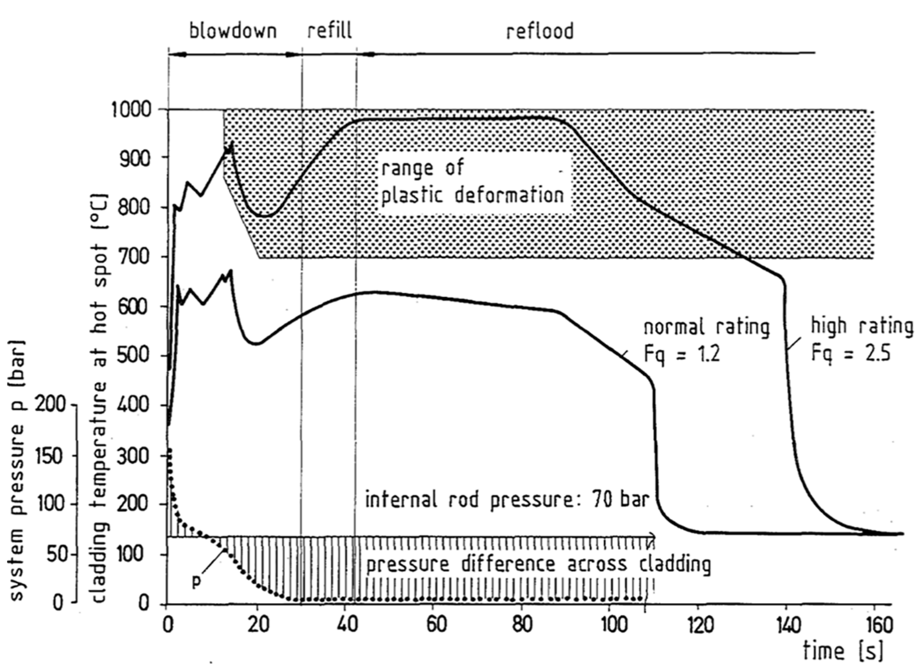


Fig. 1. Generic description of Zircaloy-4 fuel rod response during a **double-ended cold leg break LOCA** (Erbacher et al., 1987).

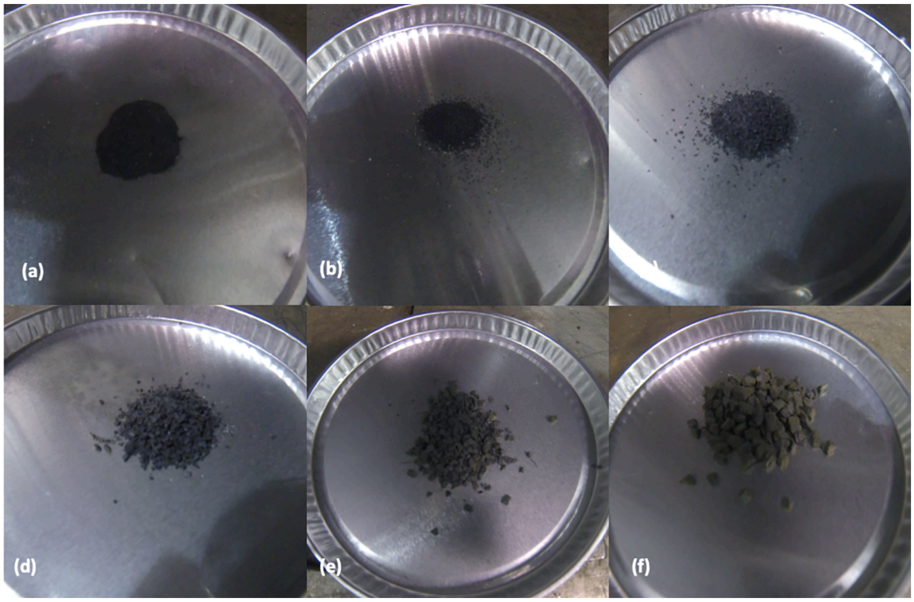


Fig. 3. Post LOCA Fuel Pulverization Images following High Burnup Integral LOCA Test Performed at the Sever Accident Test Station (Capps et al., 2020, 2021)

2. Analysis tools

2.1. VERa

VERA is a state-of-the-art reactor analysis suite of high-fidelity software and methods capable of simulating the full operating history of a commercial PWR (Kochunas et al., 2017). It includes advanced solvers and multiphysics coupling algorithms. VERA can accurately calculate fuel rod and coolant conditions at very small spatial scales, including time-dependent isotopic depletion and decay, burnable poison depletion, and in-core detector response. VERA can also simulate local impacts of control rod movement and explicit effects of fuel assembly decay and shuffling during refueling outages. VERA has been applied to nearly $^{2}/_{3}$ of the US commercial PWRs for simulation of over 200 operating fuel cycles (Kochunas et al., 2017; Godfrey et al., 2016, 2017; Salazar et al., 2016; Collins et al., 2016).

The methods employed by VERA for steady-state core follow analyses are described below. While VERA has a variety of other capabilities—including fuel rod performance, simulations of CRUD and corrosion growth in the PWR primary coolant system, and transient reactivity insertion accident analyses—the most prominent and mature capability in VERA is the simulation of nominal reactor operation. The dominant components of VERA used in this work are detailed below:

- **Mpact**: An advanced pin-resolved whole-core multigroup deterministic neutron transport capability based on the 2D/1D synthesis method on the frame of a 3D coarse mesh finite difference method, for which radial and axial correction factors are obtained from 2D method-of-characteristics (MOC) and 1D P_N, respectively (Collins et al., 2016). MPACT uses a 51 energy-group cross section library based on ENDF/B-VII.1 data and the subgroup method of on-the-fly resonance self-shielding (Development of a New 47-group Library for the CASL Neutronics Simulator," Proc. ANS MC, 2015) to obtain accurate neutron reaction rates at precise local conditions anywhere in the reactor core. MPACT has been extensively validated against a variety of critical experiments and results from continuous-energy Monte Carlo–based methods.
- **Ctf**: An advanced subchannel thermal-hydraulics capability using a transient two-fluid, three-field (i.e., liquid film, liquid drops, and vapor) modeling approach to determine the thermodynamic conditions in every coolant subchannel in the reactor core, including cross-flow effects from turbulent mixing and lateral pressure gradients (Salko et al., 2015). CTF has been validated against a broad array of single- and two-phase tests for the assessment of prediction of void generation, mixing, heat transfer, depressurization, fuel rod behavior, and droplet entrainment in a variety of different geometries.
- **Origen**: An isotopic depletion and decay code capable of characterizing used fuel (including activity, decay heat, radiation emission rates, and radiotoxicity), generating source terms for accident analyses, and activating structural materials (Gauld et al., 2011). The application programming interface enables a direct coupling to MPACT for simulation of fuel depletion and decay in millions of unique depletion regions in a reactor core. ORIGEN has been widely used and validated over the last four decades as part of the SCALE code suite (Rearden and Jessee, 2016).

2.2. Relap5-3D

The Reactor Excursion and Leak Analysis Program (RELAP5-3D) was developed at INL to model various operational transients and postulated accidents in LWR systems (Allison et al., 1993). RELAP5-3D is a systems code for analyzing thermal–hydraulic response of core and reactor coolant system (RCS) during various transients, including both small- and large-break LOCAs (LB-LOCAs). The RELAP5-3D solves two-fluid system using partially implicit numerical scheme based on a nonhomogeneous and nonequilibrium model. The code consists of various generic component models including pumps, valves, pipes, heat exchange structures, reactor kinetics, heaters, turbines, compressors, pressurizers, accumulators, and control system components. It is capable of modeling effects such as form loss, flow at an abrupt area change, branching, choked flow, boron tracking, non-condensable gas transport, two-phase flow dynamics, pre- and post-CHF heat transfer, and quench front tracking. In this study, RELAP5-3D is used to perform LB-LOCA analysis on a 4-loop Westinghouse PWR to provide systems response and transient temperature behavior of high burnup fuel to BISON.

2.3. BISOn

BISON is a fuel performance code built upon the Multiphysics Object-Oriented Simulation Environment (MOOSE) developed at INL (Gaston et al., 2009). MOOSE is a parallel finite element computational system that uses a Jacobian-free, Newton-Krylov method to solve coupled systems of nonlinear partial differential equations. The MOOSE framework provides the ability to effectively use the massively parallel computational capabilities that are needed to create high-fidelity 3D fuel rod, full-length R-Z rod, and R-θ geometric representations (Williamson et al., 2016).

BISON has been extensively validated to both LWR steady state (Williamson et al., 2016) and transient (Williamson et al., 2017) integral tests. The models used for these validation efforts are well documented in reference (Williamson et al., 2016, 2017), and each model and their implementation into BISON is well documented in reference (Hales et al., 2016). The intent of the manuscript is to leverage BISON's capabilities as outlined in (Williamson et al., 2016, 2017; Hales et al., 2016). There are, however, models very specific to LOCA (i.e., cladding burst and pulverization) discussed in more detail below.

3. Analysis methodology

3.1. LOCA thermal hydraulics

System-level best-estimate transient simulations are conducted to evaluate susceptibility of fuel pellet fragmentation and pulverization in high burnup fuel under LB-LOCA conditions. A typical 4-loop Westinghouse PWR plant with a rated power of 3626 MW is constructed using RELAP5-3D with generic 1D components, which consists of the main reactor features governing system response under the reference scenario. A generic 4-loop Westinghouse PWR RELAP5-3D model was established based on a legacy Zion model (typpwr.i). The system consists of reactor core vessel and internal components, intact and broken loops with hot and cold legs, primary coolant pumps, steam generators, pressurizer, and the ECCS (see Fig. 3). The secondary loop connected with the steam generator is modeled with main feedwater and auxiliary feedwater volumes controlling water injection boundary conditions, and with the main steam safety valves and pilot operated relief valves governing the secondary system pressure to attain desired steady-state primary coolant temperature.

For practicality, homogenized reactor core simulations are used to reduce the number of flow channels and rods. The reactor core is simulated with two pipe fuel channels connected with cross-flow junctions comprised of fifty axial nodes, implementing identical axial nodalization from VERA. The main core channel represented the average fuel rod (component number 336), and the other represented the single subchannel surrounding the rod of interest (component number 337), as the intent was to evaluate the high burnup fuel rod system response during the transient. Separate runs were conducted on four selected rods of interest. The rods were high burnup and peak temperature rods selected from various core regions. Heat structures representing fuel rods in each channel were comprised of fifty nonuniform axial nodes to capture the axial power distribution and to match the BISON meshing scheme.

Fig. 4 illustrates a simplified representation of the 4-loop Westinghouse PWR LB-LOCA model. In this study, LB-LOCA only considered a cold-leg double-ended guillotine break, as illustrated in Fig. 4 components 212, 214, and 500. It is considered the most limiting LOCA scenario, as it limits ECCS injection from the cold-leg, promotes flow stagnation, and causes ECCS injection bypass. Depressurization caused from the break initiates primary and secondary sides to trip. When the accumulator set pressure is reached, the accumulator starts injecting coolant into the primary loop. Emergency generators are initiated to operate high-pressure injection systems after a short delay. Then the low-pressure injection systems are operated to inject coolant into the reactor core to fully quench the core vessel. LB-LOCAs consist of blowdown, refill, reflood, and long-term cooling phases, all of which are modeled. HBFF susceptibility evaluations under the most limiting LOCA

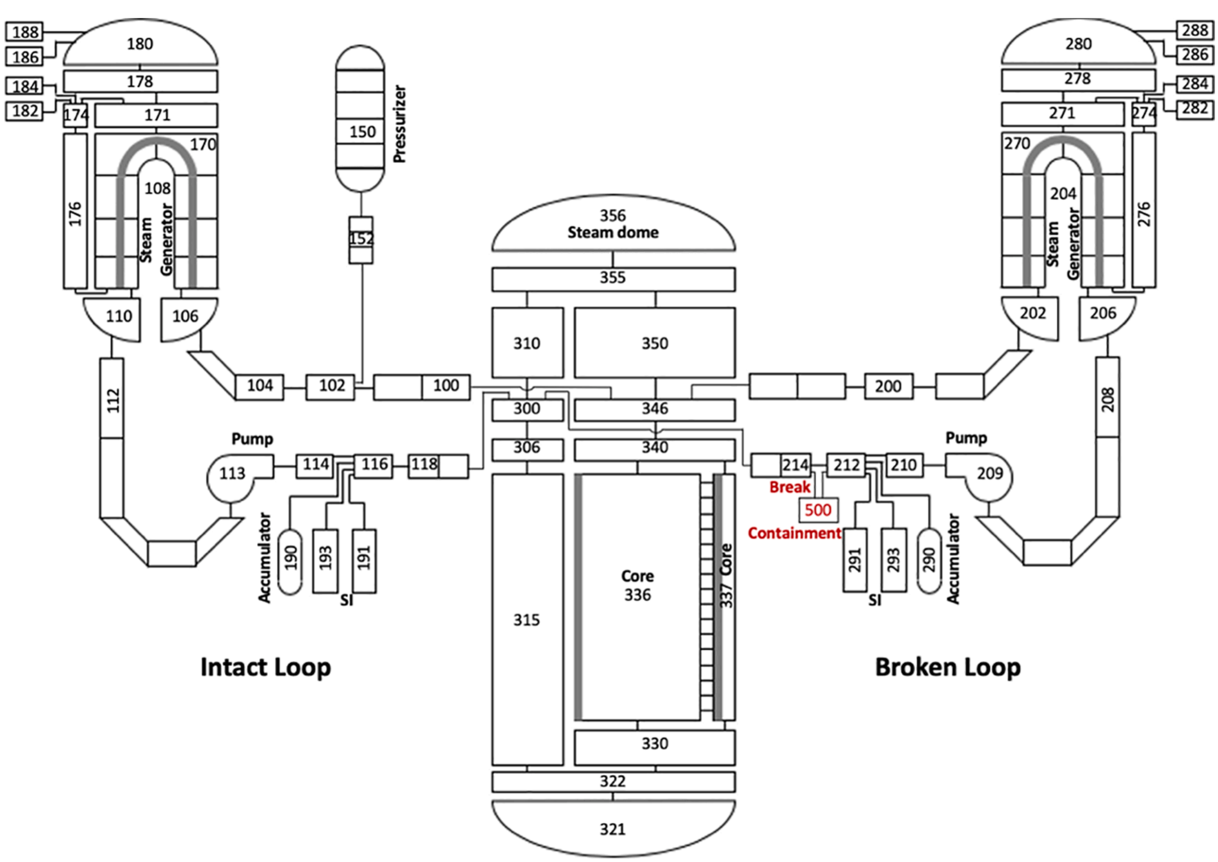


Fig. 4. RELAP5-3D 4-loop Westinghouse PWR nodalization diagram.

scenario were carried out.

The RELAP5-3D LB-LOCA model was established and compared to the LB-LOCA results in the final safety analysis report (FSAR) to ensure realistic thermal and pressure responses (Southern Nuclear Operating Company, Inc., 1997). Because the RELAP5-3D model was developed based on a Westinghouse 4-loop PWR design (Zion), the plant configuration is largely consistent with the plant reported in the FSAR. Furthermore, core geometry and operating conditions were applied in the RELAP5-3D model consistent with the high-burnup cycle design, as described below. For comparison, the nominal cycle design described in the FSAR, including peaking factor and fresh fuel thermal properties, was applied to the RELAP5-3D model. Therefore, reasonable agreement was expected between the two sets of results, and a comparison was performed to ensure that RELAP5-3D results were consistent with the reported LB-LOCA behavior. For this comparison, the built-in point kinetics model used reference peaking factors from the American Nuclear Society (ANS) Appendix K decay heat standard (American Nuclear Society, 1989).

The following four fuel pins from different assemblies were selected based on the end-of-cycle (EOC) burnup level and core location. Assembly A11 pin 3–13 has the highest rod average burnup (75.8 GWd/MTU) on the core periphery. Assembly C9 pin 7–10 is the hottest rod, with an average burnup of 38.9 GWd/MTU. Assembly E11 pin 5–4 represents the average pin power with an average burnup of 67.7 GWd/MTU. Assembly G8 pin 4–13 represents high burnup fuel (70.5 GWd/MTU) in the central regions of the core.

The steady-state power profile, the cladding surface temperature, subchannel coolant temperature, and pressure data from the MPACT and CTF calculations were implemented in the RELAP5-3D hot channel to match pretransient conditions. Based on VERA data, BISON generated the following burnup-dependent parameters for each fuel rod at EOC: rod internal pressure, number of fission gas moles, radial displacement, and pellet-cladding gap thickness. RELAP5-3D used the BISON results to inform the gap conductance model and fuel cladding deformation model to accurately calculate gap thickness, gap thermal conductivity, and pretransient fuel temperature. Subsequently, RELAP5-3D was compared to BISON to ensure that the pretransient fuel rod conditions were identical between the two codes. The Nuclear Fuel Industry Research (NFIR) correlation was used to calculate temperature dependent fuel thermal conductivity using the average fuel rod burnup (Ikeda and Kolstad, 1996). Cladding thermal properties used the cladding models documented in MATPRO (Allison et al., 1993).

3.2. Fuel performance

Fuel rod evolution under steady-state and transient conditions is critical for evaluating fuel susceptibility to HBFF. Steady-state irradiation conditions prior to the transient impacts fuel rod conditions governing the fuel rod performance during LOCA. For example, clad ballooning is governed by rod internal pressure and temperature. However, rod internal pressure is governed by cladding creep down, fuel swelling, relocation, thermal expansion, fission gas release, etc., all of which differ under various operating conditions. To capture prototypic conditions, SNC developed representative core designs to support a simulation of a 4-loop Westinghouse PWR for implementation into VERA for cycle depletion. VERA depletion results provided steady-state operating conditions at the level of the assembly and the fuel rod for subsequent thermal hydraulic and fuel performance modeling. Finally, the fuel rod model used for the LOCA analysis is a generic Westinghouse 17 × 17 fuel assembly. Details of the pre-irradiated fuel rod characteristics are shown in Table 1 (Report, 1999; Capps et al., 2018).

Calculating fuel susceptibility to HBFF was more complicated. First, time-dependent and spatially dependent fuel rod decay and thermal hydraulic boundary conditions were needed to model the LOCA transient. Time-dependent and spatially dependent decay heat profiles were calculated in VERA immediately following reactor scram for a specified period of time (approximately one hour or longer if needed). RELAP5-3D used the VERA steady-state and decay heat results to evaluate the system temperature and pressure response on the fuel rod and assembly level. VERA and RELAP5-3D results served as input and boundary conditions for BISON to calculate fuel susceptibility to HBFF. However, to calculate fuel susceptibility to HBFF, a number of criteria or thresholds were needed to evaluate the performance of the fuel throughout the LOCA.

Table 1
Preirradiated fuel rod characterization data for a Westinghouse 17 × 17 fuel assembly (Report, 1999; Capps et al., 2018).

Fuel rod parameter	Value	Units
Fuel stack height	3.6576	m
Cladding length	3.85064	m
Cladding outer diameter	9.144	mm
Cladding inner diameter	8.001	mm
Cladding type	Zr-4	
Internal gas pressure (at STP)	2.41	MPa
Gas type	He	
Fuel pellet outer diameter	7.843	mm
Fuel pellet length	9.398	mm
Fuel grain size	22	Microns
Initial density	95.67	% TD
Heat deposited in fuel	97.4	%

The methodology outlined by Capps et al. (2020b) was used to calculate fuel susceptibility to HBFF. The methodology first determines when or if burst occurs. Burst is determined by using the criterion developed by Chapman (1978), as shown in Eq. (1). Fig. 5 illustrates the burst criterion as defined by the equation for three different heating rates (0 °C/sec, 14 °C/sec, and 28 °C/sec). The cladding heating rate must be evaluated to determine which curve is most appropriate to use for each fuel rod. This could impact the timing of burst, as well as clad balloon behavior. Work performed at INL (Zhang et al., 2019) indicates that the heating rate changes as a function of time, with the initial heating rate ranging from ~24 to 34 °C/sec, and then it decreases to ~2–5 °C/sec. Therefore, each rod must be independently evaluated. Then the cladding hoop strain in the vicinity of the burst location was evaluated. Integral LOCA experiments performed at Studsvik and sponsored by the NRC indicate that the cladding can constrain HBFF. The experimental results (Raynaud, 2012) show that a cladding hoop strain of 4% at the top of the balloon will prevent HBFF, whereas the bottom of the balloon can strain to 5–6%. For the purposes of this analysis, a 4% hoop strain threshold was applied to the upper regions of the balloon, and a 5% hoop strain threshold was applied to the lower region of the balloon. This threshold requires that the cladding exceed the specified strain value in order for the fuel to become susceptible to HBFF. However, if the cladding does not exceed the threshold, then the

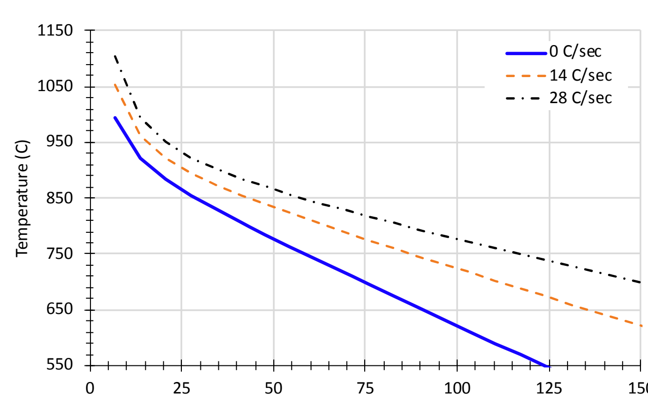


Fig. 5. Cladding burst criterion defined by Chapman (1978) for 0 °C/sec, 14 °C/sec, and 28 °C/sec.

fuel will be adequately restrained and not susceptible to HBFF. The fuel where the cladding exceeded the strain threshold was evaluated and compared to the HBFF threshold, illustrated in Fig. 6 and in equation form below. The threshold development can be found in reference (Turnbull et al., 2015). The fuel in the susceptible region was assessed on a nodal burnup and temperature level throughout the susceptible region. There may be regions of the fuel, specifically at the center of the fuel, where the local burnup and temperature are above the HBFF threshold. If this were to occur, then it would be assumed that the fuel was susceptible to HBFF if the local (i.e., nodal) temperature increased; if the local fuel temperature decreased or remained the same, then the fuel would not be considered susceptible to HBFF.

$$nopulverization \quad Bu < 70\frac{GWd}{tU}$$

$$T = -11.24Bu + 1703.8 \quad 70 \leq Bu \leq 95\frac{GWd}{tU}$$

$$T = 636 \quad Bu > 95\frac{GWd}{tU}$$

where, Bu is the nodal burnup in GWd/tU and T is temperature.

Effectively, the threshold is applied by evaluating the local state of the fuel during the transient and walking through the following steps, conceptually shown in Fig. 7, to calculate mass of fuel susceptible to HBFF. This methodology consists of four steps:

1. Compare the axial fuel temperatures to the most limiting temperature threshold.
2. Consider the impact of cladding restraint restricting HBFF.
3. Compare the radial fuel temperature and local burnup at the burst location to the threshold.
4. Calculate the total mass of fuel susceptible to HBFF.

4. Analysis results

4.1. Core design and depletion

4.1.1. Reactor and cycle design

The Westinghouse four-loop commercial PWR used for this analysis is an ambient pressure containment design with a capacity of 193 nuclear fuel assemblies and a rated power of 3626 MW_{th}. Current fuel management strategies employ 18-month fuel cycle designs with a typical low-leakage “ring-of-fire” fresh fuel layout using low-enriched uranium (LEU) fuel. Three burnup regions are typically used. 85–92 17 × 17 fresh fuel assemblies are checkerboarded with once-burned assemblies in the core interior and used in a “ring of fire” one row interior to the core periphery. The core periphery consists of once- and twice-burned assemblies. See Fig. 8. Cycles are designed to operate continuously for approximately 500 effective full power days (EFPD). Combinations of integral fuel burnable absorber (IFBA) and wet annular burnable absorber (WABA) types are used in fresh fuel assemblies to optimize power distributions and to reduce the amount of soluble boron in the reactor coolant, which is used for excess reactivity control. Silver-indium-cadmium (AIC) rod control cluster assemblies (RCCAs) are typically used for power regulating and safety functions.

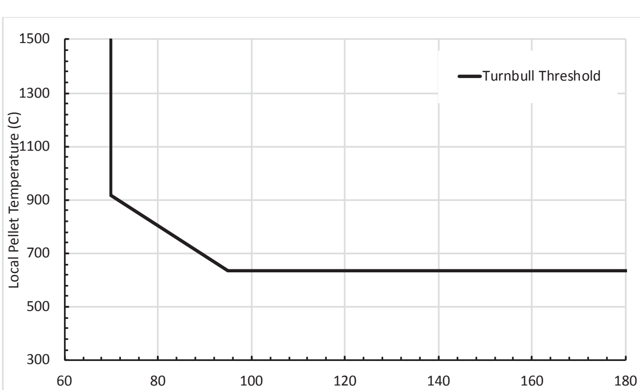


Fig. 6. HBFF temperature and burnup threshold developed by Turnbull et al. (2015).

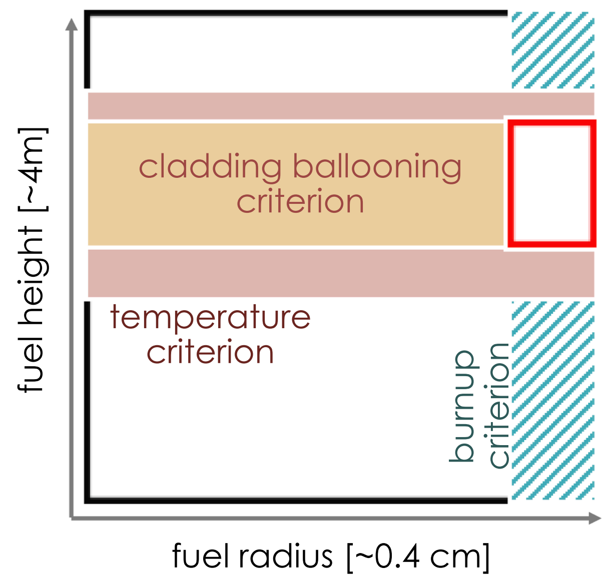


Fig. 7. The mass of the fuel susceptible to HBFF is indicated by the red rectangle. In this region, all three criteria for cladding constraint, fuel temperature, and local burnup exceed their limits (Capps et al., 2020b). (For interpretation of the references to colour in this figure legend, the reader is referred to the web version of this article.)

The process to develop a 24-month fuel cycle design used high-enrichment and high-burnup fuel built on previous core designs and depletion results. SNC developed representative loading patterns and optimization based upon their familiarity with 4-loop PWR cores and core designs. To meet all design criteria, a transition was performed from a previous core design used in a 4-loop PWR. Two cycles were transitioned from 18- to 24-month designs. In reality, it would likely take utilities additional cycles to transition to 24-month core designs, as additional fuel management and licensing concerns may prevent rapid transitions. Each of the 24-month core designs was intended to be realistic, rather than bounding all possible core designs. Ten additional 24-month core designs were developed to reach an equilibrium pattern. Representative core design results developed by Southern were compared to VERA results to ensure consistency. Design criteria employed by Southern Nuclear included:

- Fuel enrichments less than 6.95 wt% ^{235}U (beyond the current 4.95 wt% limit)
- Peak fuel rod exposure of ~ 75 GWd/MTU (beyond the current 62 GWd/MTU limit)
- Approximately 700 EFPD design cycle length
- Minimal number of once-burned fuel on the core periphery
- Approximately 84 fresh feed assemblies per cycle
- Typical low-leakage in/out ring-of-fire loading pattern
- Fh limit of 1.50 (similar to existing 18-month cycle designs)
- IFBA/WABA only for burnable poisons (no gadolinia)
- Fresh center assembly every other cycle

The design strategy incorporated a common technique of loading a single fresh assembly into the center of the reactor core and leaving it

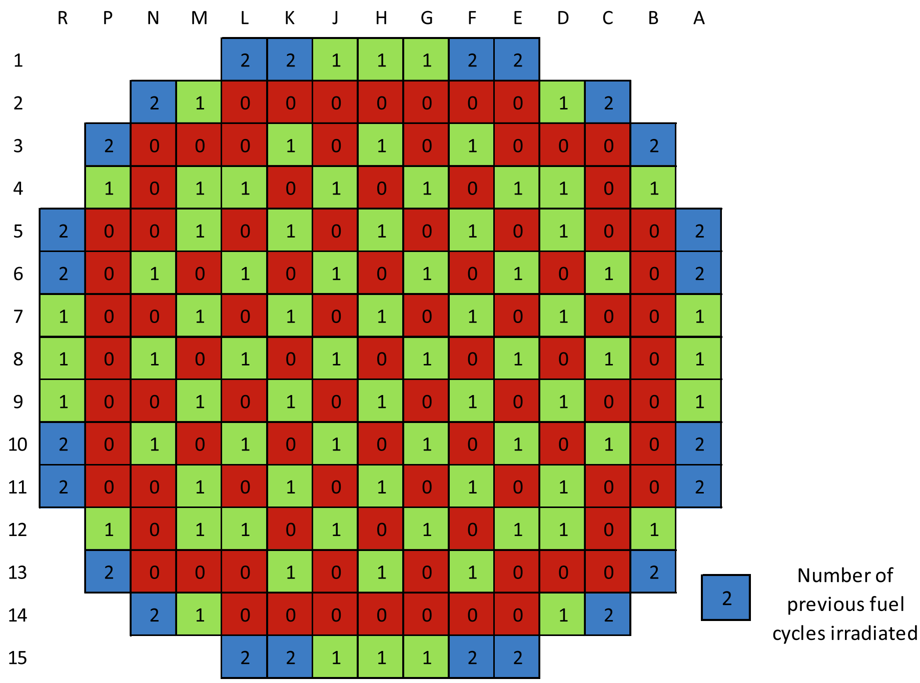


Fig. 8. Typical core loading strategy for a 4-loop Westinghouse PWR.

there for two cycles. The advantages of this strategy are that the fuel is better utilized, and fuel rod burnups are minimized in the center assembly location as opposed to breaking up symmetric assemblies for that location. However, one drawback of this approach is that twice as many cycles are needed to reach equilibrium designs. Thus, the target 24-month cycle equilibrium design is actually a pair of cycles. For this work, the cycle that was the most limiting in power and burnup was selected for downstream analysis.

To achieve 24-month cycle lengths, the fuel type was changed to a slightly “fatter” (i.e. larger diameter) fuel rod design to accommodate ~ 9% more fuel. Another difference included using a slighter shorter intermediate spacer grid with a reduced form loss coefficient. This fuel transition was implemented during the first transition cycle and beyond with the inclusion of increased enrichment.

4.1.2. Equilibrium cycle design generation

Representative transition and high burnup core designs were developed by Southern Nuclear Operating Company (SNC) to provide realistic and feasible patterns based on current reload design and safety analysis constraints. In the future, more detailed analyses may reveal some problematic characteristics such as high boron concentration, CIPS susceptibility low shutdown margin, and adding RCCAs and control rod drive mechanism in spare slots, but these were not considered for this analysis. The primary goal for this work was to generate power histories for high-enrichment, high-burnup fuel for subsequent LOCA and FFRD analysis. Table 2 provides cycle design information relating to the loading pattern generation. Note that the design strategy for the center assembly resulted in pairs of equilibrium cycles, and Table 2 core designs do not represent the actual designs in operation. Cycle HBu10 was selected as the equilibrium pattern for further analysis.

4.1.3. HBu10 cycle design characteristics

Cycle HBu10 achieved a 693 EFPD cycle length and a peak fuel rod average burnup of 76.9 GWd/MTU. The core loading pattern for the equilibrium cycle is shown in Fig. 9 in quarter-core symmetry, with enrichment and burnable poisons provided for the feed fuel, and previous cycle locations provided for the reinsert fuel. VERA-calculated BOC and EOC peak pin burnups are provided in Fig. 10, and the rod-wise burnup distribution is provided in Fig. 11. Fig. 12 provides the critical boron letdown for the equilibrium cycle, as well as the maximum fuel rod relative power (FΔH). Fig. 13 displays the radial rod-wise power distribution from VERA at the burnup where it reaches the maximum

Table 2
Transition and equilibrium 24-month cycle design information.

Cycle	Cycle length (EFPD)	Number of feeds	Feed enrichments (wt% ^{235}U)	Number IFBA/ WABA	Maximum fuel rod FΔh	Maximum fuel rod burnup (GWd/ MTU)
T1*	522.7	92	4.60/4.95/5.20	11616/400	1.46	56.1
T2ˉ	695.1	93	5.20/5.60/5.95	16024/784	1.48	61.6
HBu1^{+}	700	92	5.60/5.95/6.20	16480/848	1.47	70.2
HBu2	693	89	5.60/5.95/6.20	16072/736	1.46	70.5
HBu3	693	84	5.95/6.20/6.60	16096/592	1.49	73.1
HBu4	693	81	5.95/6.20/6.60	15128/592	1.49	73.1
HBu5	693	84	5.95/6.20/6.60	16032/624	1.50	78.1
HBu6	693	85	5.95/6.20/6.60	16056/704	1.49	75.5
HBu7	693	84	5.95/6.20/6.60	16032/608	1.49	73.3
HBu8	693	85	5.95/6.20/6.60	16056/704	1.50	76.8
HBu9	693	84	5.95/6.20/6.60	16032/608	1.49	74.3
HBu10	**693**	**85**	**5.95/6.20/6.60**	**16056/704**	**1.50**	**76.9**
HBu11	693	84	5.95/6.20/6.60	16032/608	1.49	74.3
HBu12	693	85	5.95/6.20/6.60	16056/704	1.50	75.9

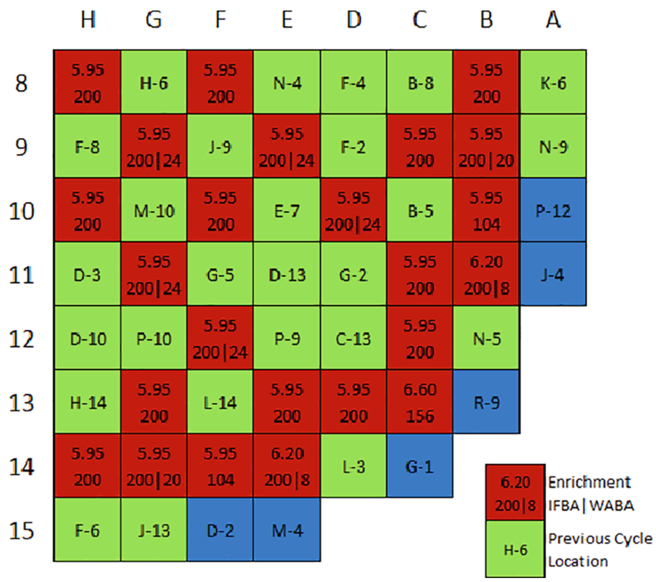


Fig. 9. Cycle HBu10 core loading pattern.

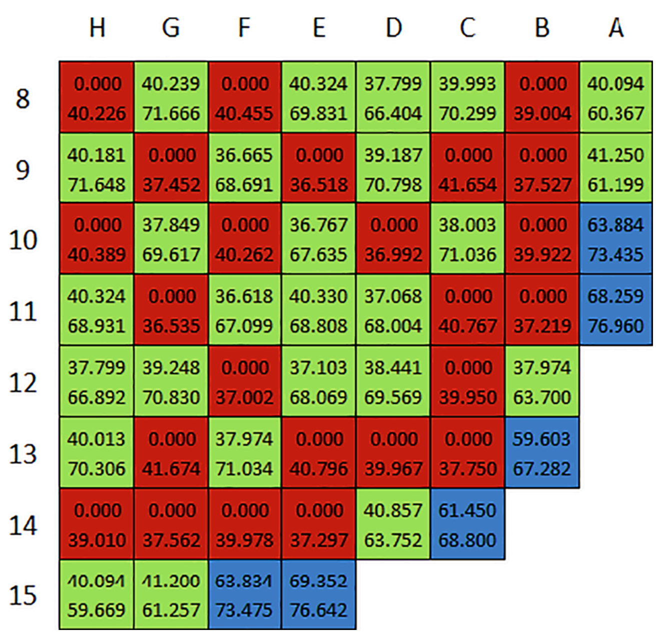


Fig. 10. Cycle HBu10 BOC and EOC peak pin exposures (GWd/MTU) for fresh (red), once burned (green) and twice burned (blue) fuel assemblies. (For interpretation of the references to colour in this figure legend, the reader is referred to the web version of this article.)

FΔH. Lastly, VERA Cycle HBu10 results were compared to the methods used by Southern Nuclear to ensure consistency between several parameters of interest. The comparison is shown in Table 3 and demonstrates remarkable agreement considering the large difference between the two models and methods.

4.2. LOCA thermal hydraulics

4.2.1. Benchmark comparison to 4-Loop Westinghouse PWR

We conducted an assessment of our 4-Loop Westinghouse PWR model to ensure that our baseline results were in good agreement with the PWR FSAR. The PWR FSAR system parameters, including the rated power, peaking factor, and accumulator and pressurizer set points, were implemented in the model for a consistent comparison. A parametric

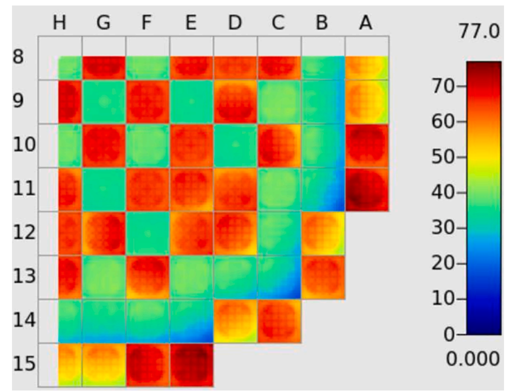


Fig. 11. Cycle HBu10 EOC fuel rod exposures (GWd/MTU).

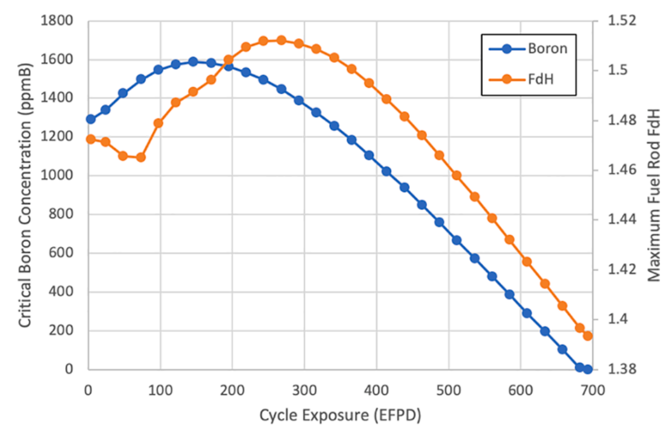


Fig. 12. Cycle HBu10 critical boron concentration and maximum fuel rod relative power.

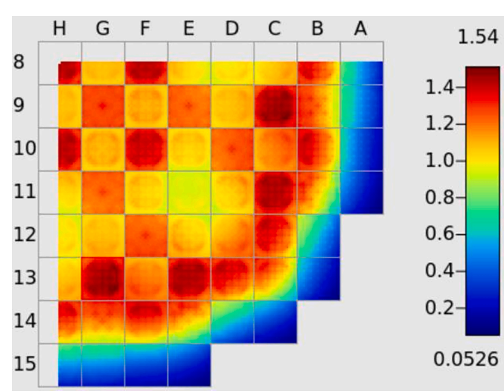


Fig. 13. Radial rod-wise power distribution at the burnup where maximum FΔH occurs.

Table 3
Comparison of Utility Simulation and VERA results for equilibrium 24-month cycle design.

Parameter	Utility Simulation	VERA	Percent difference (%)
Maximum fuel rod power	1.49	1.51	0.8
Maximum local pin power	1.76	1.85	5.3
Maximum assembly power	1.39	1.39	0.0
Maximum fuel rod burnup	76.8	76.9	0.1
Maximum boron concentration	1568	1586	0.01

study of four discharge coefficients (0.4, 0.6, 0.8, and 1.0) was conducted to investigate break flow and depressurization trends over the coefficients. Both break flow and depressurization from the RELAP5-3D model showed comparable behavior to the FSAR results. The most limiting case of 1.0 discharge coefficient is considered in this study. A large break is initiated at 0 s, releasing the pressurized coolant through the cold leg as shown in Fig. 14(a). Loss of coolant causes the pressure loss of the reactor core vessel, where the average core depressurizes from 15.6 MPa to the atmospheric as illustrated in Fig. 14(b). The model's overall break flow and system depressurization responses are in good agreement with the results reported in the FSAR, shown in Fig. 14(a). The baseline model was built based on the reactor core condition implemented from the VERA analysis results. We implemented the power profile provided from the VERA results as axial power shape for large break LOCA was not reported in the FSAR, which impacted the pre-transient steady-state cladding temperature difference of 25 °C. Pre-transient flow condition prompted by the reactor core condition and the circulation pump showed mass flow difference in the break loop by 500 kg/s. Hence, a small discrepancy in the break flows during the blowdown phase is observed during the time from 0 to 2.5 s in Fig. 12(a). The break flow difference triggered the RELAP5-3D analysis to delay the core average depressurization compared to the FSAR results as shown in Fig. 12(b). The RELAP5-3D analysis results for the break flow and the average core pressure are compatible with the FSAR, following the typical LBLOCA blowdown behavior. Though the reactor safety components were set to values reported in the FSAR, specifics and geometries on each component can vary and cause minor different refill responses. The core quench front of the model is illustrated in Fig. 14(c), which showed more conservative results in the present model compared to the response in the FSAR. Fig. 14(d) shows the cladding temperature response at the peak cladding temperature (PCT) location during the LB-LOCA. The PCT was reached at 200 s and 250 s for PWR FSAR and the present RELAP5-3D model, respectively. The discrepancy in the applicable time of PCT occurred due to the difference in the aforementioned pre-transient conditions and the system response during the blowdown, refill, and reflood. The PCT responses showed good agreement. The reported PCTs were 1029 °C and 1023 °C for RELAP5-3D and the PWR FSAR, respectively, as shown in Fig. 14.

4.2.2. High burnup LOCA analysis

Fig. 15 shows the G8 high burnup fuel pin EOC steady-state coolant, cladding surface, and fuel temperatures. RELAP5-3D and CTF show axial coolant temperature increase along the long the axial length of the fuel rod, verifying appropriate energy deposition and heat transfer from the fuel rod to the coolant channel. The RELAP5-3D cladding surface temperature results closely agree with the CTF results. Note that VERA models heat transfer enhancement effect downstream of spacer grids, resulting in localized decreases in cladding temperature, causing the jagged effect seen in Fig. 15(b). This effect is not modeled in RELAP5-3D, resulting in a smoother axial temperature profile across the cladding.

BISON and RELAP5-3D fuel centerline temperatures are in excellent agreement. Compared to VERA average fuel temperatures, the RELAP5-3D fuel average temperature is more conservative, as it accounts for the burnup-dependent gap thermal properties (i.e., fission gas thermal conductivity degradation). The same approach and steady-state analysis were conducted for all fuel rods; the results for each case show similar trends as those observed for G8. The comparison of the coolant temperature ensured correct implementation of mass flow rate and power

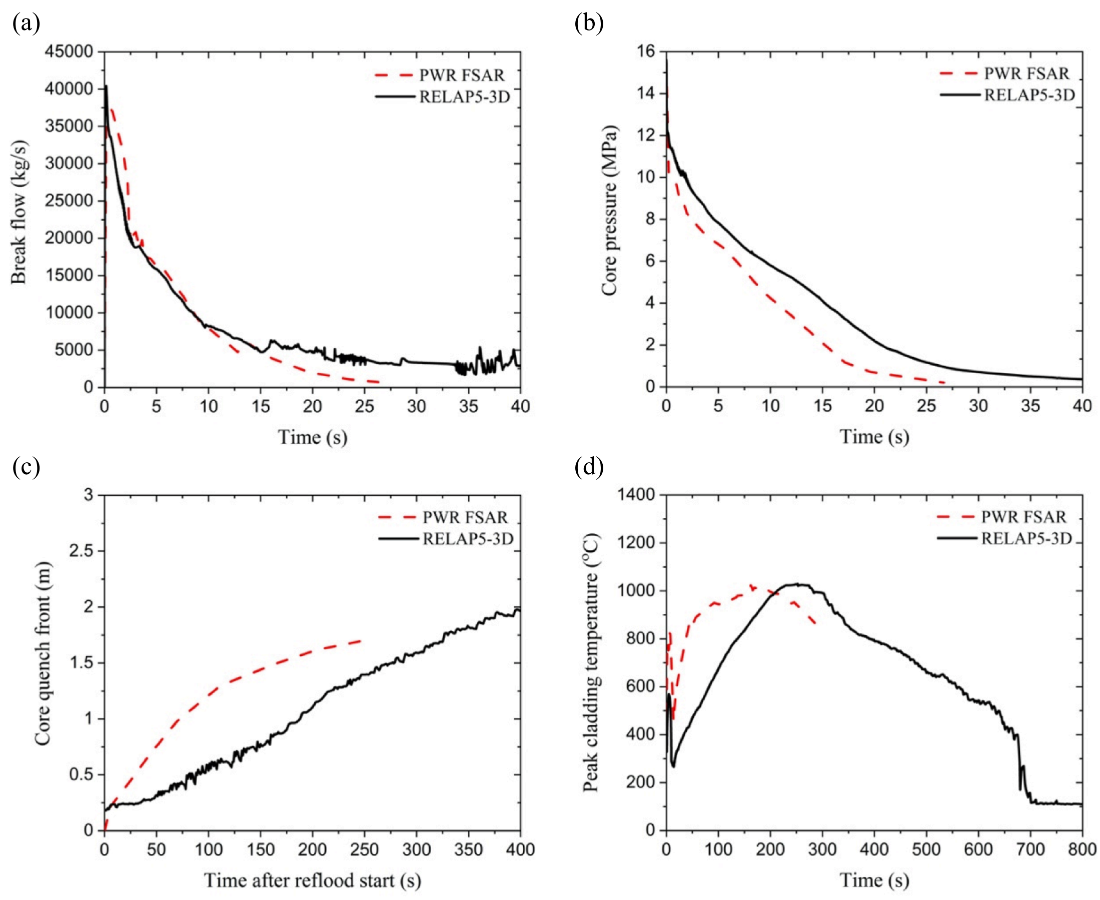


Fig. 14. The Vogtle FSAR and representative RELAP5-3D simulation comparisons of (a) break flow, (b) core pressure, (c) core quench front, and (d) peak cladding temperature.

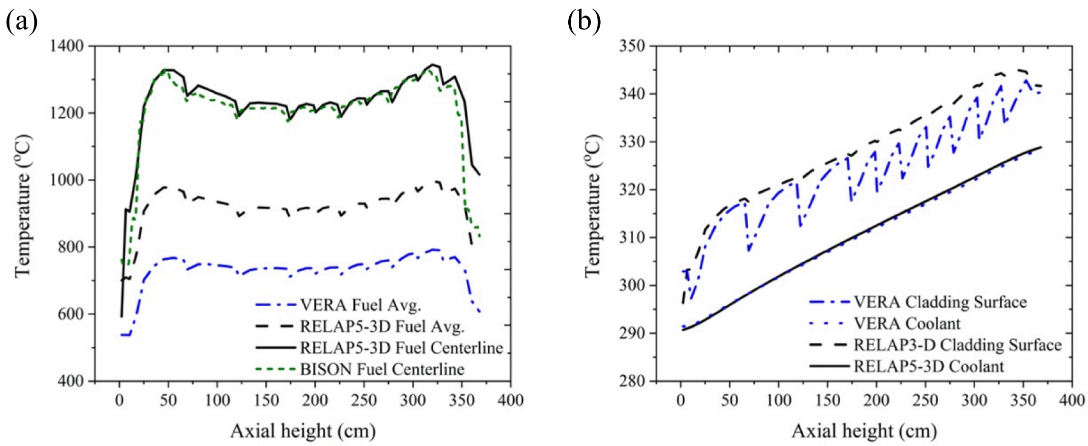


Fig. 15. The RELAP5-3D, VERA, and BISON steady-state fuel rod data integration for G8 high burnup fuel pin at the center core: (a) fuel temperature and (b) coolant and cladding surface temperatures.

output via the fuel rod. Furthermore, the comparable cladding surface and fuel centerline temperatures provided ensure that pretransient conditions are consistent in the fuel system conduction calculation.

Following rupture of the cold-leg, the maximum fuel temperature rapidly decreased from the steady-state operating temperature, as seen in Fig. 16(a). Over time, the cladding temperature increases to the PCT reported in Table 4, which reports the peaking factors, PCT, and local burnup for all fuel rods considered. Time-dependent cladding surface heat flux response at the PCT location, as shown in Fig. 16(b), is impacted by heat generation rate, as well as the thermal–hydraulic conditions in the adjacent coolant. During the blowdown and reflood phases, the heat flux rapidly decreases due to the scram-induced decrease in fuel heat generation, and subsequently, the loss of liquid coolant and the decrease in steam flow through the core. The decay heat generated by VERA is applied as a function of time, as shown in Fig. 16(c and d). The applied decay heat sustains the cladding temperature until

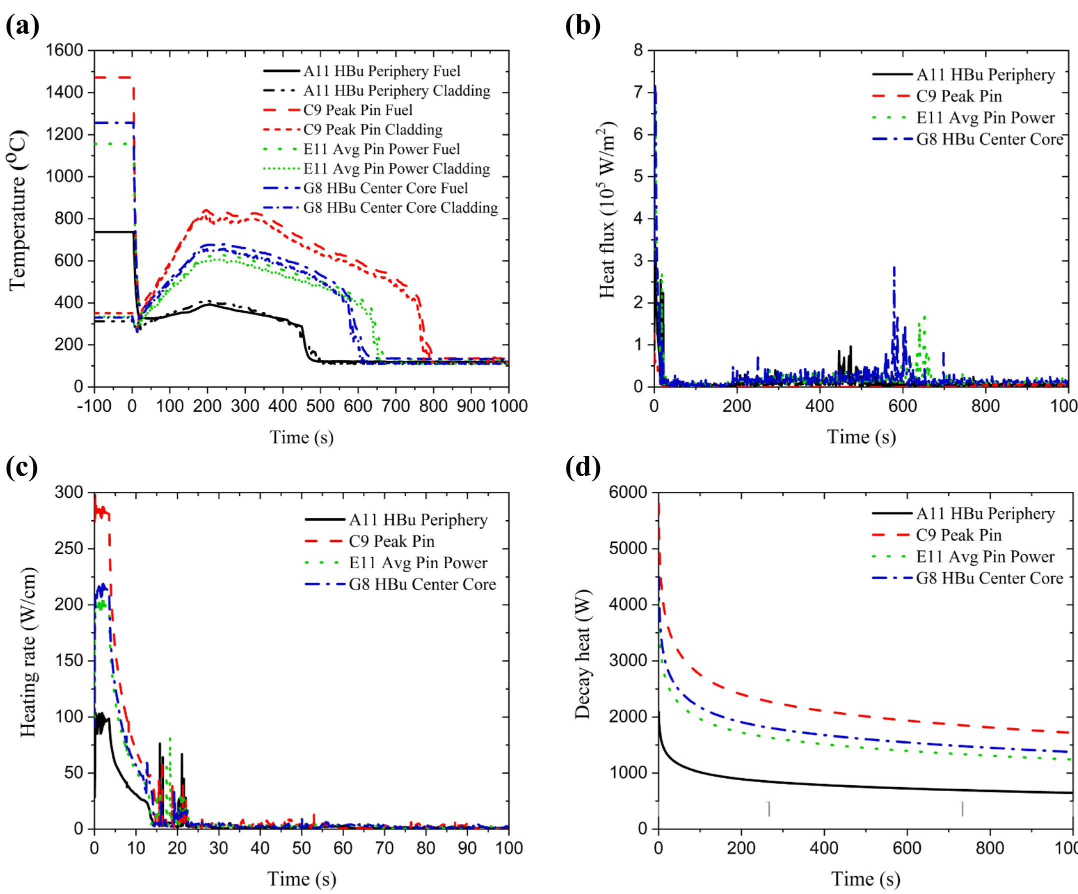


Fig. 16. (a) Fuel centerline and cladding surface temperatures, (b) heat flux, (c) heating rate **at the PCT location, and (d) decay heat versus time for a LB-LOCA at EOC.**

Table 4
Steady-state rod average peaking factor, PCT, and local burnup at PCT location for considered fuel rods.

Assembly fuel rod	Peaking factor	PCT	Local burnup
A11 high burnup core periphery	0.48	410 °C	81.1 GWd/MTU
C9 peak temperature pin	1.34	821 °C	40.7 GWd/MTU
E11 average pin power	0.94	608 °C	71.6 GWd/MTU
G8 high burnup core center	1.04	656 °C	75.5 GWd/MTU

the quench phase, which occurs at ~600 s for the high burnup fuel pin, at which point the heat flux rises due to rewetting and reflood coolant boiling. The heat flux then decreases to ~1.5 × 10^4 W/m^2 as fuel temperatures stabilize during post-reflood conditions.

4.3. Fuel performance

4.3.1. Steady state results

4.3.1.1. Operating conditions. Three fuel rods were identified from the VERA full core depletion analysis for subsequent BISON steady-state and transient analyses. The VERA analysis calculated power and cladding surface temperatures at 98 axial locations. The primary factors for selecting fuel rods were assembly/pin location, rod average burnup, and rod average peaking factor. As a result, two fuel rods from the center of the core and one fuel rod from the periphery of the core were evaluated. One additional fuel rod from assembly C9 was evaluated. This fuel pin had the highest peaking factor, ~1.34, at EOC, with a rod average burnup of 38 GWd/MTU, which resulted in the highest PCT. This fuel rod was intended to serve as a bounding condition for PCT, burst timing, etc. The details of this fuel rod are not listed below since it is not a high burnup fuel rod.

The first fuel rod is Assembly G8 Pin 4–3 (G8). The complete rod average linear heat rating (LHR) for Assembly G8 Pin 4–3 is shown in Fig. 17. Assembly G8 was originally located in the F8 position, effectively in an adjacent position. The assembly was moved to G8, directly adjacent to the center assembly, and was surrounded by fresh assemblies. This resulted in the Assembly G8, and more specifically Pin 4–3, operating at high powers for the duration of the cycle, resulting in the highest rod average burnup in the center of the core. The fuel rod accumulated a rod average burnup of 71.56 GWd/MTU during two cycles of operation. Lastly, the dips in the figure are consistent with end-of-cycle refueling outages.

The second fuel rod was located in Assembly A11 Pin 3–13, and the complete rod average LHR is shown in Fig. 18. Assembly A11 was initially located at the L14 position on the ring of fire. Adjacent assemblies were either fresh or twice burned, and as a result, fuel rods adjacent to the fresh fuel assemblies operated at higher power than fuel rods adjacent to the twice-burned assemblies. In particular, Pin 3–13 was located in the assembly region near fresh fuel assemblies.

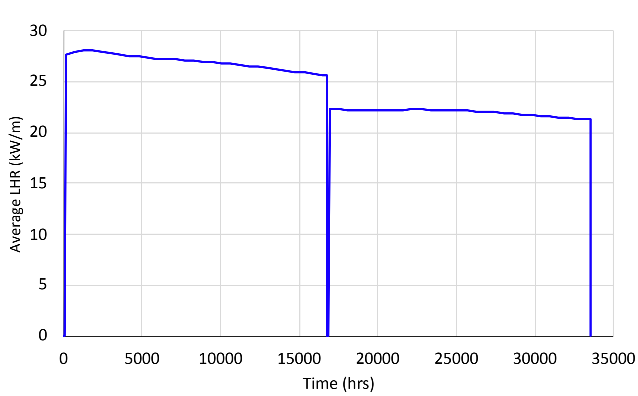


Fig. 17. Rod average LHR for fuel rod located in Assembly G8 Pin 4–3.

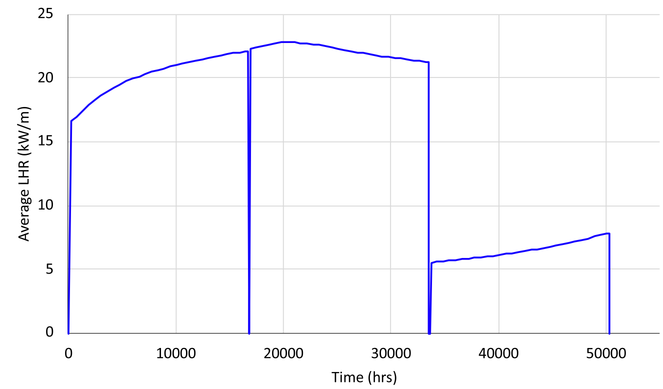


Fig. 18. Rod average LHR for fuel rod located in Assembly A11 Pin 3–13.

Subsequently, the assembly moved inward, just inside the ring of fire, adjacent to fresh fuel assembles and near once-burned fuel assemblies. Again, Pin 3–13 was regionally located near fresh fuel assemblies, resulting in higher pin powers and additional burnup accumulation as compared to other pins in the assembly. Finally, assembly A11 was moved to the periphery of the core. Pin 3–13 was adjacent to the ring of fire and accumulated the highest rod average burnup in the core at 76.88 GWd/MTU.

The final fuel rod was located in Assembly E11 Pin 5–4; Fig. 19 illustrates the average LHR for the fuel rod throughout irradiation. Originally, the assembly was located in the ring of fire at D13, where the fuel pin and assembly experienced very high LHRs. The assembly was subsequently moved to E11 and was surrounded by once-burned fuel assemblies. Over time, the average LHR reduced over the course of the cycle. Outside of the fuel rods on the periphery of the core, Assembly E11 had the lowest peaking factors of the high burnup assemblies, accumulating a rod average burnup of 68.8 GWd/MTU.

4.3.1.2. Steady state analysis results. Fuel rod evolution during steady-state operation is critical for evaluating fuel performance during LOCA conditions at or near end of life. Furthermore, fundamental fuel performance parameters such as rod internal pressure (RIP) and fuel centerline temperature are important for ensuring that fuel rod integrity is maintained throughout normal operation and anticipated operational occurrences. NUREG-0800, Chapter 4.2, specifically states that RIP should remain below the system pressure and should also account for gas generated though the fission and neutron absorption process (NUREG-0800, 2021). The reviewer guide also states that alternative limits could be provided but must be justified. NUREG-0800, Chapter 4.2, also requires fuel pellet overheating to be evaluated (NUREG-0800, 2021). The reviewer guide states that analyses should be performed for

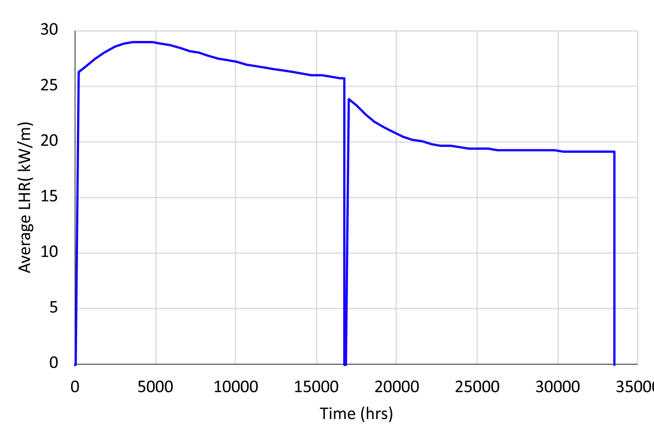


Fig. 19. Rod average LHR for fuel rod located in Assembly E11 Pin 5–4.

the maximum LHR throughout the core. RIP and fuel centerline temperatures are not only dependent on each other, but they are also highly reliant on fuel rod operating conditions and material models defining fuel rod evolution. Therefore, it is important to evaluate those parameters contributing to evolution of the parameters of interest over the course of operation. Numerous evaluations are likely needed to adequately address the safety case; however, BISON analyses are intended to highlight potential areas of concern or areas for which additional analyses would provide insightful results. BISON was used to evaluate steady-state performance of each fuel rod prior to the LOCA analysis. Those parameters important to safety were evaluated in order to understand the state of the fuel rod during normal operation. Fig. 20 compares rod internal pressure BISON results as a function of time and as-fabricated fuel grain radius. For reference, typical UO_2 fuel grain radii are ~ 11.5 μm ("Effect of Startup Ramp Rate on Pellet-Cladding Interaction of PWR Fuel Rods" EPRI Technical Report TR-, 1999). The BISON results indicate that rod internal pressure could be a serious concern for high burnup fuel rods with smaller grains of 5.75 μm radially. For prototypic grain dimensions, RIP begins approaching the system pressure (~15.5 MPa) and requires more in-depth analyses over a larger range of operating conditions.

The impacts of fuel and cladding mechanical changes on RIP are the simplest mechanisms to understand and evaluate. During the irradiation process, UO_2 is subject to a number of phenomena resulting in mechanical deformation (thermal expansion, relocation, swelling, etc.), whereas cladding mechanical changes are primarily driven by irradiation creep. Thermal expansion, elastic deformation, and thermal creep all play secondary roles. Fuel and cladding mechanical changes, with the exception of swelling, impact RIPs early in life, prior to gap closure. Fuel swelling is a constant process resulting from the fission process, when fission products gradually expand the fuel matrix. In the absence of fission, fuel swelling would not occur. Time-dependent fuel and cladding radial strain results are shown in Fig. 21. Strain results were taken at the axial location corresponding to the end-of-life peak centerline temperature, (i.e., ~3.13 m from the bottom of the active fuel). As indicated in the figure, fuel and cladding radial strain results for high-powered fuel rods (G8 and E11) converge at ~ 10 GWd/MTU. This convergence is consistent with pellet-cladding gap closure. Referencing back to Fig. 20, RIP for all rods early in life increase at a constant rate while the gap is open. Following gap closure, RIP appears to increase at a faster rate for the high-powered fuel rods (G8 and E11), whereas pin 3–13 in assembly A11 maintains a relatively constant increase in RIP. This constant increase persists until ~ 32–34 GWd/MTU, at which point the RIP begins to increase at a faster rate.

The sudden increase in RIP can be directly correlated to fission gas release (FGR), as seen in Fig. 22. Fuel rods in assemblies G8 and E11 operate at very high LHRs, or temperatures, for the majority of operation, resulting in the generation and release of large amounts of fission gas, whereas pin 3–13 in assembly A11 operates at lower LHRs relative to the other analyzed pins. This operating regime delays FGR until fuel temperatures and/or burnup are high enough for fission gas to successfully release into the plenum. Furthermore, improving UO_2 microstructure by increasing the grain size could be a safety benefit and a potential economic benefit as indicated in Fig. 20, Fig. 22, and Fig. 23. Reducing FGR improves the fuel rod's thermal response in high burnup conditions, as shown in Fig. 23. RIP and FGR are both decreased by simply increasing the radial size of the grains from 5.75 to 11.5 μm. FGR is dependent on fission gas atoms diffusing through the grain to the grain boundary, or free surface, at which point gas atoms are released to the plenum. Increasing the size of the grain increases the diffusion distance,

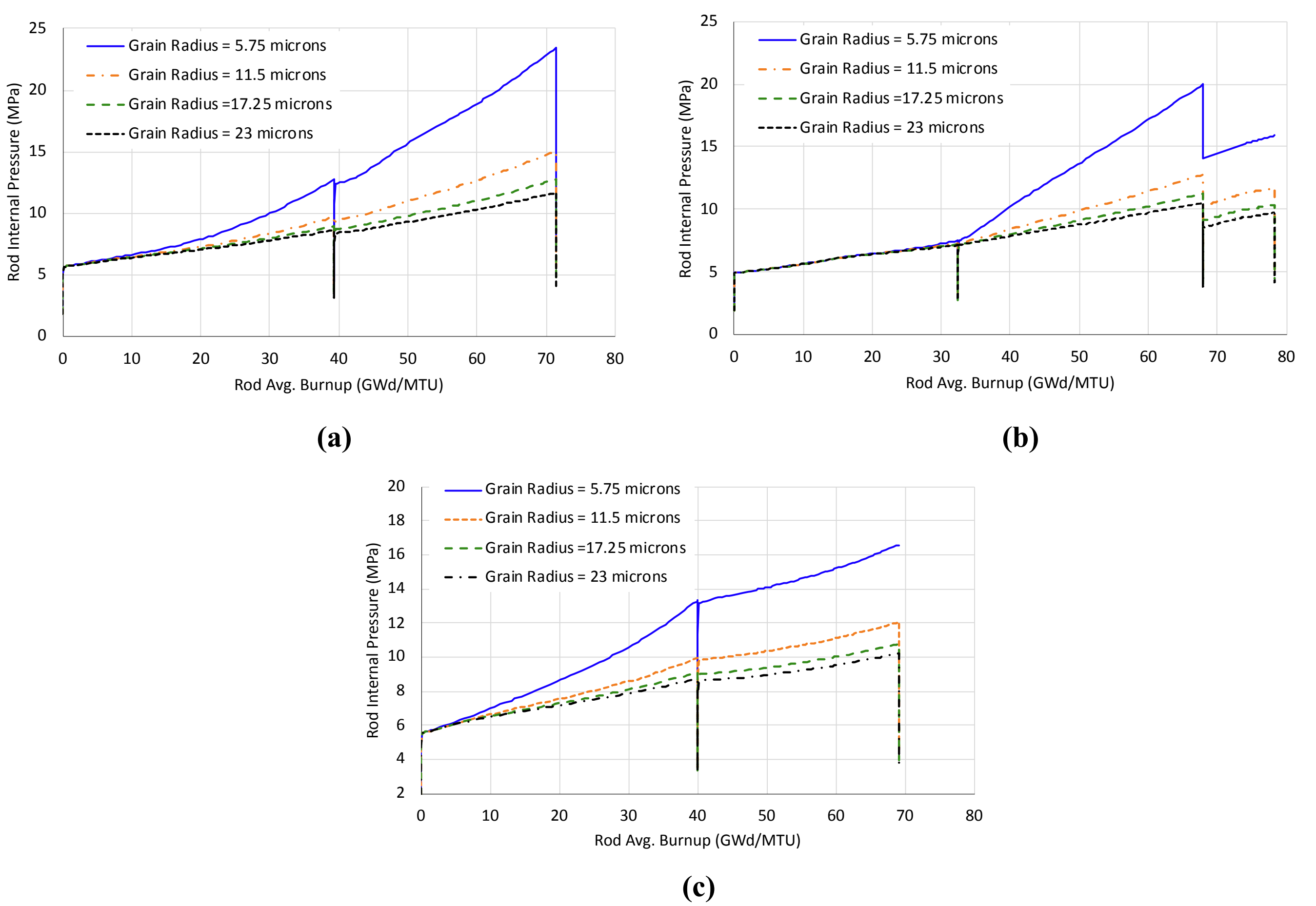


Fig. 20. Time-dependent RIP results for (a) G8 Pin 4–3, (b) A11 Pin 3–13, and (c) E11 Pin 5–4.

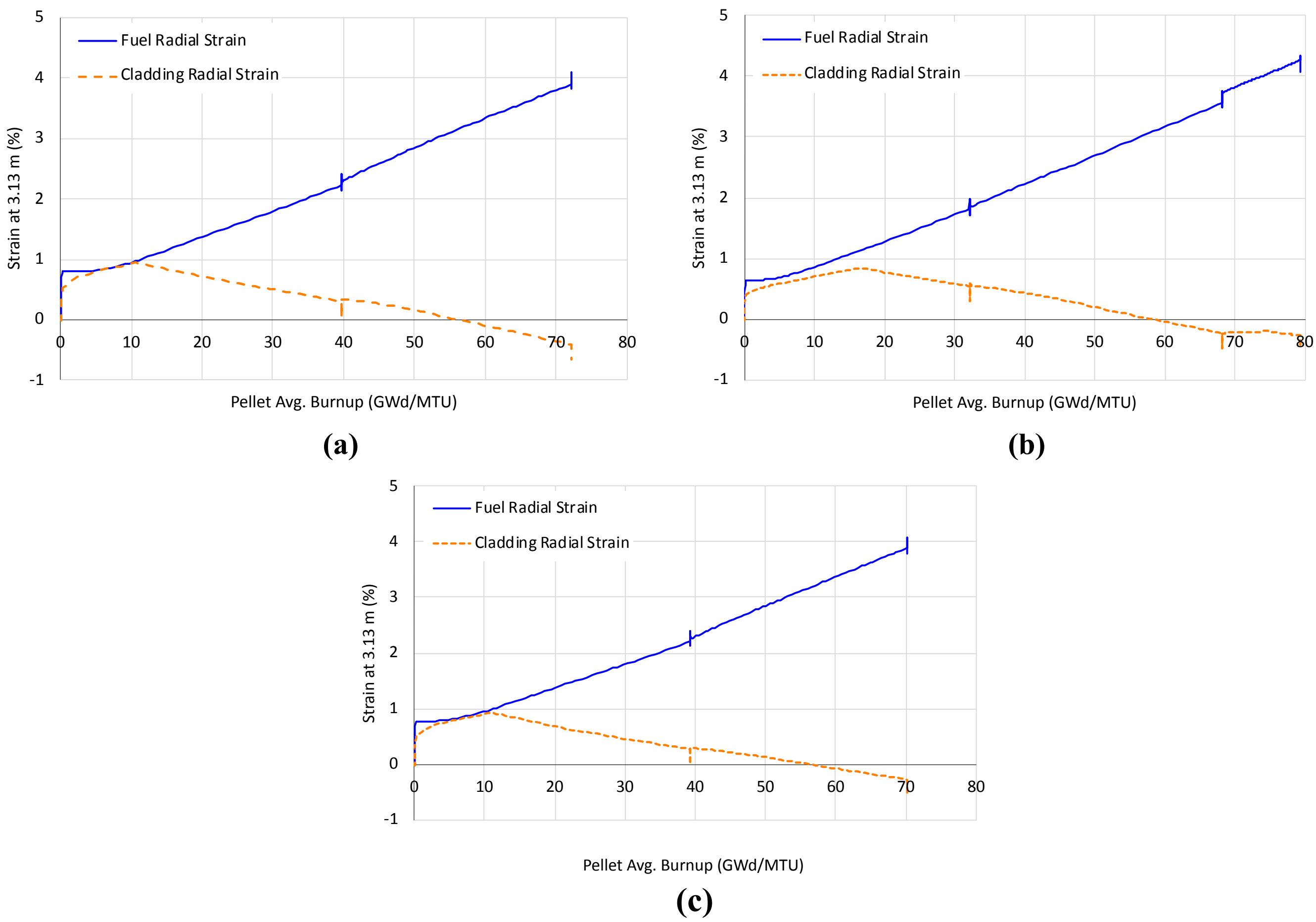


Fig. 21. Time-dependent fuel and cladding radial strain results at the axial location 3.13 m from the bottom of the fuel for (a) G8 Pin 4–3, (b) A11 Pin 3–13, and (c) E11 Pin 5–4.

and therefore delays FGR.

Released fission gas contaminates thermal conductivity across the gap and further increases fuel temperatures. By reducing FGR, high burnup fuel operates at lower temperatures, thereby improving the high burnup fuel performance and providing additional safety margin to fuel melt during overpower events. However, increasing fuel temperatures and burnup can increase FGR release rates, but this was not observed for larger grain boundaries under the analyzed operating conditions. Operating conditions have a strong impact on FGR, and burnup extension will likely result in more aggressive operating conditions in conjunction with increasing burnup. Therefore, it is important to continue investigating FGR and its impact on fuel performance under high burnup operating conditions.

4.3.1.3. Pre-Transient fuel conditions. Currently, mechanisms driving high burnup fuel pulverization and subsequent relocation and dispersal are up for debate. Experimental data to date indicates that fuel pulverization is strongly dependent on local burnup (i.e., nodal burnup and pellet average burnup) and terminal temperature (Turnbull et al., 2015). Experimental conditions to date have used electrically heated or partially electrically heated systems to heat rodlets to LOCA temperatures. At this writing, there has not been an experimental effort designed to replicate pretransient fuel conditions, as well as the evolution of the fuel conditions during a LOCA. One of the key outcomes of this work is to provide experimentalists with prototypic pretransient and transient fuel conditions to support experiments designed to replicate high burnup LOCA conditions observed in PWRs. It has been hypothesized that the evolution of the fuel pellet stress state during the LOCA contributes to pulverization, so understanding stress evolution across the fuel pellet radius may provide some additional insights to mechanisms contributing to pulverization.

Fuel operating temperatures are among the primary parameters for consideration. Over the course of operation, fuel temperatures across the rod (radially and axially) change as operating conditions evolve. When removing operating conditions and considering a constant power, fuel temperatures initially decrease as the pellet-cladding gap closes. Once gap closure occurs, fuel temperatures continue to increase due to UO_2 thermal conductivity degradation. This occurs until the local pellet burnup exceeds ~ 60 GWd/MTU, at which time thermal conductivity degradation impact on fuel temperatures is relatively small for a constant LHR. However, FGR begins contaminating gap thermal conductivity, and fuel temperatures continue to increase, as seen in Figs. 21–23. Making the problem more complex, strong fuel temperature and burnup gradians form across the ~ 4 mm pellet radius, and as a result, local (i.e., center, middle, and rim) regions in the pellet form, with each region containing different microstructural features and mechanical properties.

During normal operation, these burnup and temperature distributions are manageable and do not appear to have detrimental effects. However, a LOCA causes local fuel temperatures to diverge from equilibrium, with the fuel centerline temperature decreasing, and fuel temperature on the periphery increasing, potentially at different rates. Experimentalists trying to replicate LOCA fuel performance must understand prototypic operating conditions (burnup and temperature distribution) to ensure appropriate fuel conditions prior to the LOCA. Fig. 24 highlights local fuel conditions at peak power location prior to the LOCA transient. Fig. 24a illustrates radial fuel temperatures as a

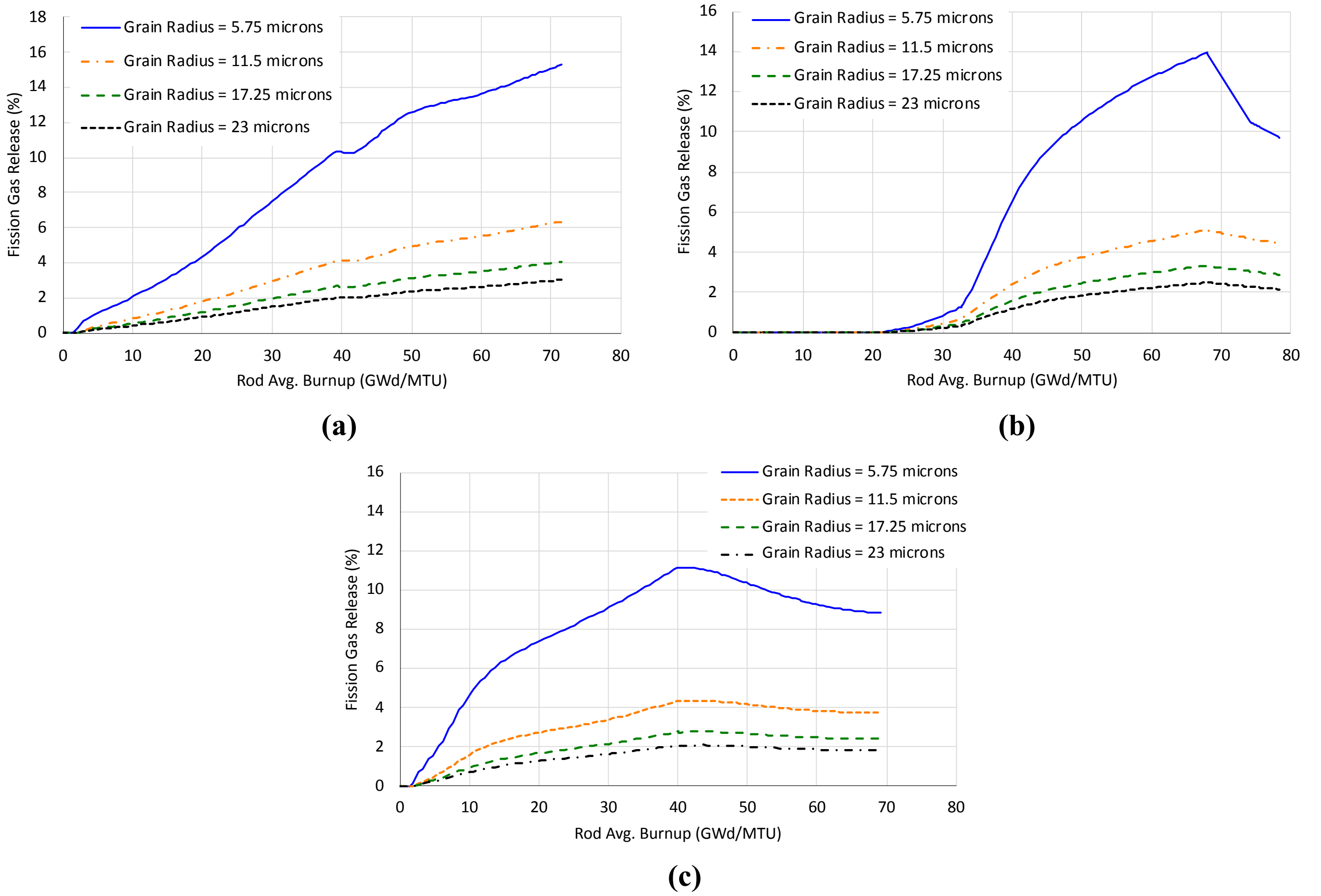


Fig. 22. Time-dependent FGR results for (a) G8 Pin 4–3, (b) A11 Pin 3–13, and (c) E11 Pin 5–4.

function of radial location, while Fig. 24b illustrates radial fuel temperatures as a function of the radial burnup profile. Fuel operating conditions can be divided into two bins and are consistent with the results reported by Zhang et al. (2019). The two bins are associated with the location of the fuel rods in the core: the center of the core and the periphery of the core. VERA results indicate high burnup fuels operating in the center of the core have a rod average peaking factor of ~ 0.9–1.1 (~17–21 kW/m), and the fuel centerline temperature is expected to range from ~1000 to 1350 °C. Operating conditions—radial temperature and burnup—shown in Fig. 24a represent the operating conditions observed in VERA and are comparable to results reported by Zhang et al. (2019).

Assembly A11 has the lowest temperature profile, as this fuel rod is located on the periphery of the core, where fuel rods operate at low LHRs with respect to fuel rods in the center of the core. As shown in Figs. 24b and 5, fuel centerline temperatures are below the cladding burst criteria. This indicates that burst may not occur in fuel rods on the periphery of the core, but transient thermal hydraulic and fuel performance analyses are required to confirm. Again, operating conditions observed in this work are consistent with operating conditions identified by Zhang et al. (2019), so it is reasonable to assume that the conditions plotted in Fig. 24 can serve as an upper or near upper bound pretransient condition for fuel rods operating on the periphery of the core. It should be noted that these conditions may only be valid for 24-month 4-loop Westinghouse PWRs, as plant operating conditions and cycle lengths could impact fuel operating conditions. However, these results can serve as a reasonable assumption until high burnup core designs and operating conditions are available for different types of PWRs, as well as for 18-month high burnup fuel cycles.

Formation of radial and circumferential cracks in UO_2 has been observed and understood under reactor startup, power changes, and shut down conditions (Aleshin et al., 2010; Capps et al., 2018, 2017, 2016; Zhang et al., 2018; Jiang et al., 2020). Furthermore, UO_2 cracking has been evaluated using various analysis techniques such as finite element and extended finite element to assess the impact of UO_2 cracking on the overall fuel performance (Capps et al., 2018, 2017, 2016; Zhang et al., 2018; Jiang et al., 2020; Reliability, 2008; Analyses and Recommendations, 2006; Chapman, 1978). Based on these analyses, it seems reasonable to assume that changes in the fuel pellet stress during a LOCA contributes to pellet pulverization. Fig. 25 evaluates pretransient hoop, radial, and axial stresses for the fuel pin from assembly G8. The fuel pellet is under hydrostatic compression. The center of the pellet is in compression, and physically, the center of the pellet is the hottest region, and it thermally expands in all directions more than other regions of the pellet. This thermal expansion behavior results in the cooler regions of the pellet applying compressive stresses on the center region of the pellet. Moving radially toward the pellet periphery, hoop and axial stresses gradually become less compressive and effectively neutral. The expectation would be for tensile stresses to form on the pellet periphery, resulting in cracking of the pellet to reduce stresses. However, cracking is currently not considered; creep is primarily governing the stress state across the pellet. Radial stress is driven by thermal expansion with no external force acting in the radial direction, and therefore, fuel in the radial direction will be in compression throughout the pellet, with the pellet's outer surface being effectively stress free. At the pellet's periphery, hoop and axial stresses become more compressive. This stress transition is a result of hard pellet-cladding contact. Hard pellet-cladding contact is a result of swelling and higher temperatures increasing the interaction force between the fuel and cladding. The fuel begins to push radially outward while the cladding pushes

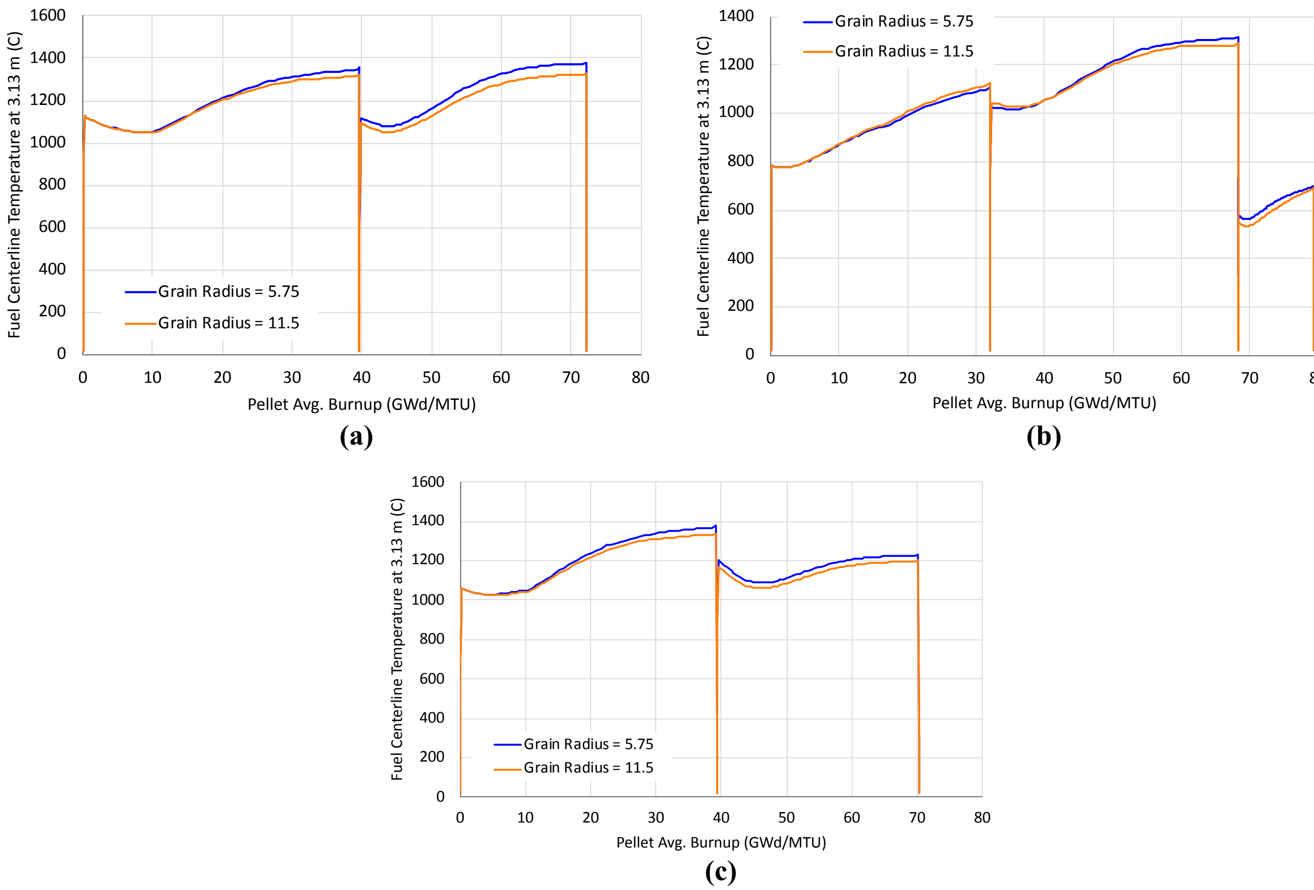


Fig. 23. Time-dependent fuel centerline temperature results at the axial location 3.13 m from the bottom of the fuel (a) G8 Pin 4–3, (b) A11 Pin 3–13, and (c) E11 Pin 5–4, indicating lower temperatures with less fission gas.

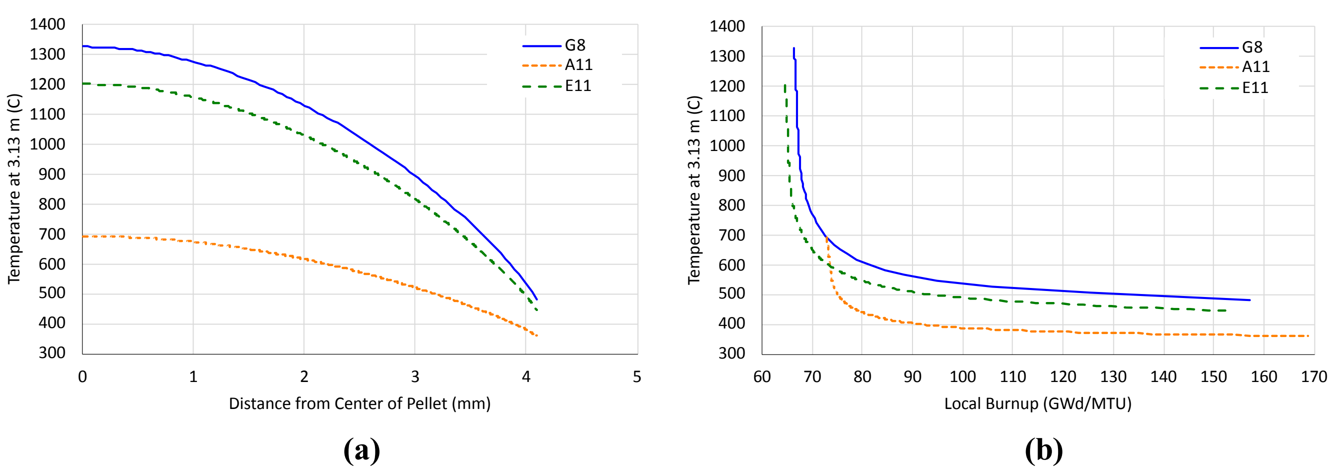


Fig. 24. Pre-transient fuel (a) temperature profile as a function of radial position, and (b) temperature profile as a function of radial burnup at the peak power location (~3.13 m from the bottom of the active fuel).

radially inward. This interaction results in additional compressive stresses on the fuel pellet periphery.

4.3.2. Transient results

Cladding burst is the deciding factor for whether or not fuel dispersal occurs during a LOCA. If cladding burst does not occur, then the fuel, even if pulverized, remains in the fuel rod and avoids release. If the cladding does burst, then fuel dispersal into the reactor coolant system becomes a possibility. The aforementioned fuel rods were evaluated for potential burst under simulated LOCA conditions. Because neither of the high-burnup fuel rods reached the temperatures and stress conditions necessary to burst, an additional once-burned fuel pin from assembly C9 was evaluated.

Cladding burst was evaluated by comparing the calculated cladding temperature to the burst criteria defined in Fig. 5. The cladding is assumed to have burst when the cladding temperature and stress conditions align with Fig. 5. Fig. 26 comparesC9 Pin 7–10 cladding temperature and hoop stress at the burst location to the burst criteria. In this

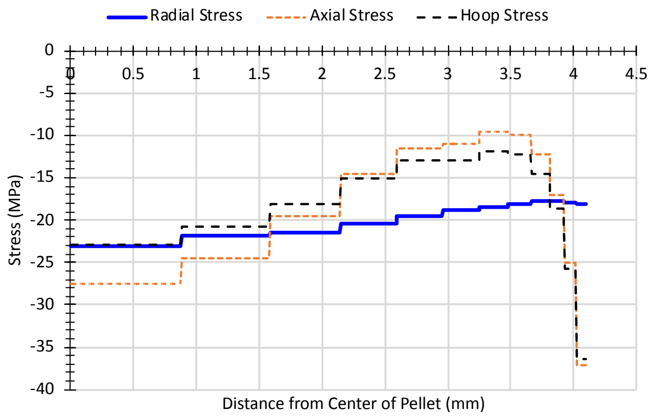


Fig. 25. Pretransient hoop, radial, and axial stress profile in the fuel at the peak power location of G8 Pin 4–3.

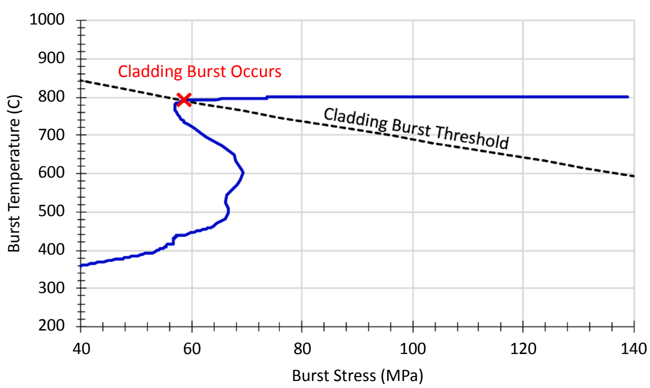


Fig. 26. Burst temperature as a function of burst stress for C9 Pin 7–10 (peak temperature rod).

case, burst occurs once the cladding temperature reaches 790 °C, which is higher than the reported high burnup LOCA integral test (Wiesenack, 2013; Lekkonen, 2007; Oberlander et al., 2011; Raynaud, 2012; Flanagan et al., 2013). High burnup LOCA tests indicated that cladding burst occurs between ~675–750 °C. There are reported burst tests in which the cladding did not burst until the temperatures observed in the BISON simulation were reached. However, pretransient rod internal pressures for these experiments were significantly less, whereas the pretransient rod internal pressure was near the system pressure of ~ 12.3 MPa. Burst test on full length rods may be worth investigating this discrepancy.

Rodlets used in integral LOCA experiments and cladding burst test use similar cladding radii and thicknesses, but individual rodlets are typically 0.3 m long. Commercial fuel rods, however, are significantly longer (~3.6 m) than these rodlets (0.3 m). Because commercial fuel rods have a much larger internal volume than test rodlets, rod internal pressure decreases at a faster rate as a balloon forms. This sustained pressure allows ballooning to occur axially along the cladding, with increased deformation corresponding to higher temperatures. Fuel rod temperatures uniformly increase until the reflood process begins quenching the fuel rod from bottom to top. As the reflood process progresses, local axial regions of the fuel rod cool, terminating the local ballooning process. This results in subsequently smaller changes to the fuel rod's internal volume and pressure conditions. Consequently, upper spans of the cladding tube are subject to increased temperatures for a longer period of time, allowing larger balloon sizes to form, and increasing the likelihood of burst.

Fig. 27 shows cladding hoop strain and temperature as a function of axial location along the fuel rod at the time burst occurs. Due to higher temperatures, there is noticeably more cladding deformation, and burst

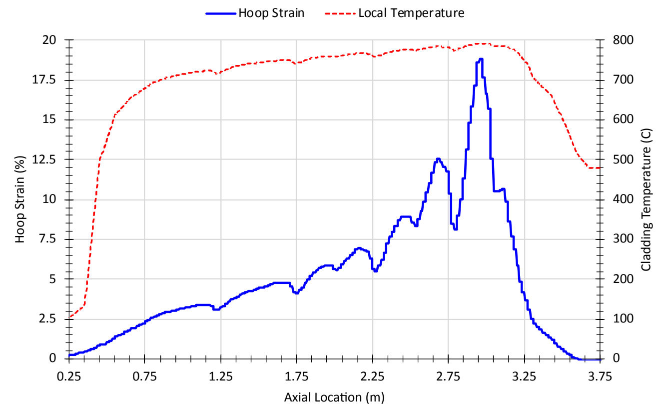


Fig. 27. Hoop strain and cladding temperature profile as a function of axial location at the time burst occurred for C9 Pin 7–10 (hot rod).

occurs in the upper regions of the fuel rod. Hoop strain depressions are located near spacer grids and/or mixing vanes which serve as a structural member, stabilizing the fuel rods. Cladding temperatures in these regions are noticeably less. Localized lower temperatures result in localized decreases in cladding hoop strain during the LOCA. It should be noted that mixing vanes and spacer grids were not explicitly modeled in BISON, but they were included in VERA to generate steady-state conditions (cladding temperature, axial power, etc.) used in these simulations. Effectively, the neutronic and thermal hydraulic effects of spacer grids are included, but the structural features like constraint are not. These results are expected to change by including the structural restraint of spacer grids and associated conditions during the LOCA; the addition of a mechanical constraint will lead to deeper depressions along the hoop strain profile.

Peak hoop strain and axial lengths of the cladding balloon are consistent with experimental observations (Erbacher et al., 1987, 1990; Billone et al., 2008; Erbacher and Leistikow, 1985; Wiesenack, 2013; Lekkonen, 2007; Oberlander et al., 2011; Raynaud, 2012; Flanagan et al., 2013; Askeljung et al., 2012). Fig. 28 provides a comparison of cladding burst temperature as a function of hoop strain for a variety of cladding burst test experiments, and Fig. 29 shows the axial cladding hoop strain profile of rodlet 192 from the NRC LOCA test program. The peak hoop strain indicates that cladding failure would have been likely to occur for these cladding temperatures at nearly the same time by assessing a strain-based failure criterion instead of the burst temperature model used in this simulation. The peak hoop strain observed for this simulation is ~ 19% at a temperature of nearly 790 °C, consistent with

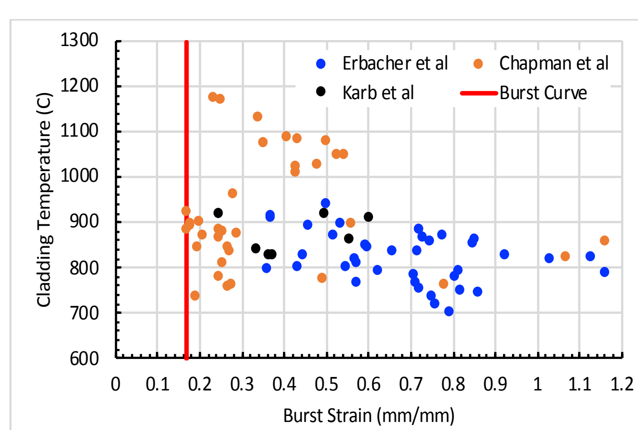


Fig. 28. Experimental peak hoop strain data generated through various cladding burst test programs (Erbacher and Leistikow, 1985; Erbacher et al., 1990; Karb, et al., 1980).

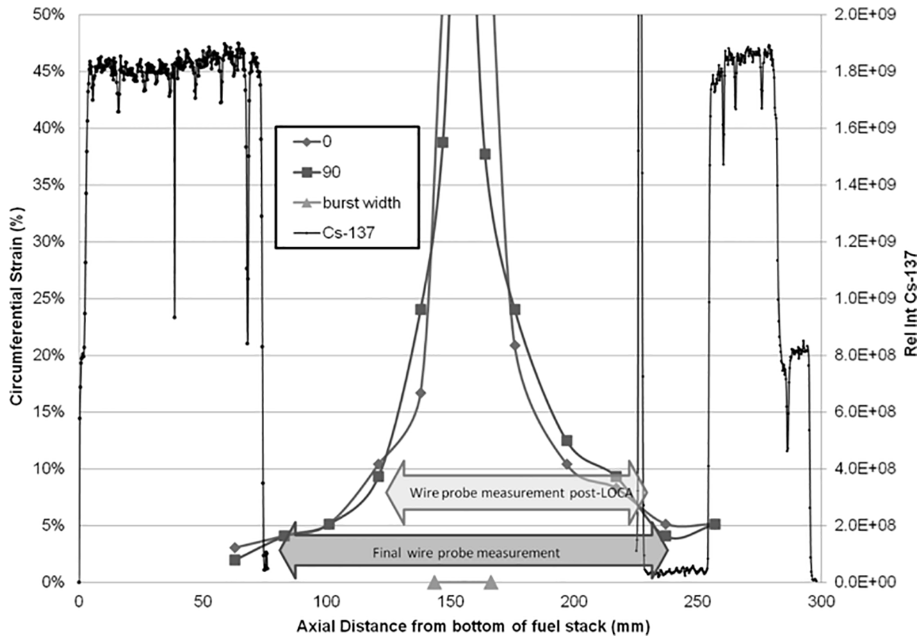


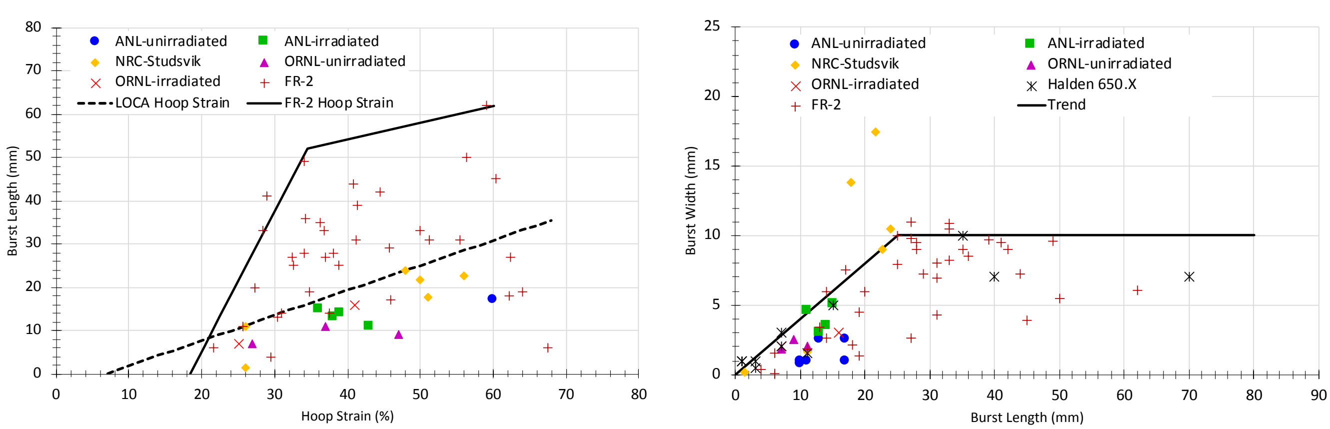
Fig. 29. Hoop strain distribution and post LOCA fuel loss measurements as a function of axial location for NRC LOCA rodlet 192 (Raynaud, 2012; Flanagan et al., 2013).

experimental results shown in the figure. The balloon length calculated from BISON is consistent with the length between spacer grids, ~22.8–25.4 cm, while Fig. 29 indicates the balloon length is between ~10 and 15 cm. BISON simulation results are conservative with respect to the LOCA test, but explicitly modeling the spacer grids may improve the accuracy of balloon length calculations.

The size of the burst opening may impact the amount of fuel susceptible to dispersal from the cladding. Therefore, it is important to estimate the size of the burst opening by correlating the size of the cladding balloon to the burst opening. Fig. 30 shows experimental data used to develop a correlation between the length of burst length to balloon hoop strain (Fig. 30a) and between burst length and burst width (Fig. 30b). Scatter in the data makes it difficult to determine a conservative length, so two correlations were considered. The first correlation is an upper bound as a function of hoop strain and is driven by the FR-2 data. The second is correlated to the high burnup LOCA integral test data. BISON predicts a peak hoop strain of ~ 19%. Mapping this strain to the correlations seen in Fig. 30a indicates that the burst length is expected to range from 0 to 6.5 mm. Burst width data (Fig. 23b) show a clear correlation to burst length. Mapping a conservative burst length of 6.5 mm to Fig. 30b indicates that the expected width of the burst opening is ~ 3 mm. This would indicate that the area of the burst is ~ 19.5 mm^2. Considering the area of a fuel pellet from the side is ~ 82 mm^2, the burst opening is ~ 23% the size of a fuel pellet. Fuel dispersal has primarily been experimentally observed and not measured, but the observations indicate that fuel dispersal becomes significant once the area of the burst opening is larger than the fuel pellet (Wiesenack, 2013; Lekkonen, 2007; Oberlander et al., 2011; Raynaud, 2012; Flanagan et al., 2013; Askeljung et al., 2012). Experimental observations indicate that small amounts of pulverized fuel disperse when the area of the burst opening is less than the area of a pellet. However, it is not possible at this time to quantify the amount dispersed from the opening.

4.3.3. *Fuel susceptibility to HBFF*

Determining fuel susceptibility to HBFF requires knowing the pellet average burnup and terminal, hottest temperature at the burst location.



(a) **(b)**

Fig. 30. Burst (a) length and (b) width correlation to the peak hoop strain observed in LOCA test programs (Karb, et al., 1983; Southern Nuclear Operating Company, Inc., Vogtle Electric Generating Plant Unit 1 and Unit 2, 1997; Wiesenack, 2013; Lekkonen, 2007; Oberlander et al., 2011; Raynaud, 2012; Flanagan et al., 2013; Askeljung et al., 2012).

Radial burnup and temperature profiles can then be mapped to the Turnbull pulverization model to calculate the depth of fuel which may pulverize. The thermal hydraulic analysis indicated that burst would occur in the region near the peak power, which, for high burnup fuel rods, occurs at ~ 2.7 m from the bottom of the fuel. Fig. 31 shows the average fuel pellet burnup distribution at the expected burst location for every rod exceeding a rod average burnup of 62 GWd/MTU. The bulk of the pellet burnup ranges from 70 to 72 GWd/MTU, with the highest burnup being 77 GWd/MTU. It should be noted that the burnups listed below are rounded up to the nearest burnup for conservatism.

Fuel temperatures were calculated and used to determine pulverization susceptibility. Fig. 32 shows the calculated percentage of fuel susceptible to pulverization for average fuel pellet burnups ranging from 63 to 77 MWd/kgU based on the Turnbull pulverization model summarized in Fig. 6. Two temperatures were considered for this evaluation based on those calculated by RELAP5-3D in Section 4.2.2: 656 and 821 °C. 656 °C is consistent with the RELAP calculated PCT for high burnup fuel rods in the center of the core. Additional high burnup fuel rods were modeled in RELAP5-3D, and the results indicated that lower temperatures would be observed, as well. Higher temperatures may occur as every rod was not evaluated, so a bounding case was developed using the highest power fuel rod at EOC (C9). The PCT for this fuel rod was 821 °C. This temperature will serve as a bounding temperature, as it is likely one of the hottest rods in the core. Fuel susceptibility to pulverization between the two fuel temperatures range from 1 to 16% susceptibility; 1% susceptibility is assumed to account for pulverization occurring in the pellet rim region. Pulverization may not occur in this region. However, to conservatively estimate the amount of fuel pulverized, it is assumed that at least 1% of the pellet will be susceptible to pulverization for all burnups and temperatures observed. The worst-case scenario observed is 16%, which occurs only in the highest temperatures and at pellet average burnups > 73 GWd/MTU. This is consistent with Fig. 32 where one can observe a sharp increase in pulverization susceptibility once the fuel burnup exceeds 73 GWd/tU and when the terminal temperature is 821 °C.

Coupling the cladding response discussed in the previous section to the results in Fig. 32 allows for calculation of fuel pulverization susceptibility throughout the core. BISON and integral LOCA experimental results determine the burst length, which influences the length of fuel susceptible to pulverization. The NRC LOCA data suggest that the susceptibility region ranged from 10.16 to 15.24 cm, or 4–6 in., whereas BISON indicates that the susceptibility region spans between spacer grids at 24.94 cm, or 9.8 in.. Again, BISON results are expected to overpredict fuel pulverization susceptibility compared to the experimental results. The total fuel volume susceptible to pulverization can be calculated by multiplying the susceptible cross-sectional area by the axial length of 10–25 cm. Susceptible fuel mass is calculated by multiplying the total susceptible fuel volume by the density of UO_2. It should be noted that fuel susceptibility does not indicate the occurrence of HBFF; it simply means that the local burnup and temperature conditions exceeded the prescribed threshold and may experience HBFF.

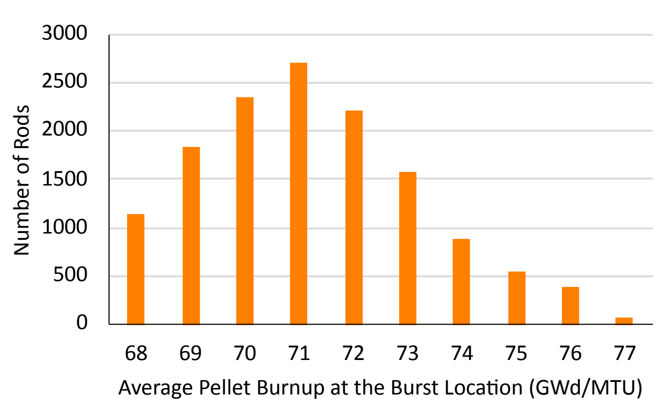


Fig. 31. Pellet average burnup distribution at the burst location for every rod that exceeded a rod average burnup of 62 GWd/MTU.

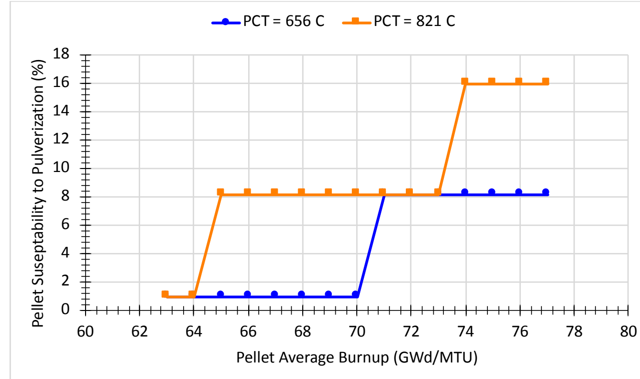


Fig. 32. Percentage of fuel pellet susceptible to pulverization in percent for the best estimate and core-wide PCT over the burnup range of interest.

Table 5 summarizes the results. The BISON best estimate analysis indicates that 106.4 kg of fuel will be susceptible to pulverization; when accounting for uncertainties and assuming the worst-case scenario, this value increases to 182 kg. However, experimental data suggest that the length of the cladding balloon is expected to be smaller than that predicted by BISON, so the amount of mass susceptible to pulverization is expected to decrease, as well. Another implication not considered is the impact of delayed burst. Burst was not predicted to occur in any of the high burnup fuel rods. Burst only occurred for the once-burned fuel rod near the PCT of 790 °C. Experimental data indicate that hydrostatic pressure can reduce pulverization susceptibility by ~ 40% (Turnbull et al., 2015). This relationship is observed in Fig. 33, which has been normalized and reproduced from reference (Turnbull et al., 2015). However, additional experimental data are needed to investigate the implications on the fuel rod level. The fact still remains that delaying burst until higher temperatures are reached will likely decrease the amount of fuel susceptible to pulverization. However, it is not possible to quantify this effect at this time.

4.3.4. Fuel pellet conditions during a LOCA

The previous section describes pretransient fuel conditions, but analyses targeting the fuel mechanical evolution during a LOCA are sparse. Fig. 34 illustrates the radial temperature profile time history during a LOCA, and Fig. 35 illustrates a radial stress (radial, axial, and hoop) distribution as a function of time during a LOCA. Fuel temperatures decrease from steady-state temperatures to the coolant temperatures with in ~ 5 s, as shown in Fig. 34. During the blowdown period, thermal hydraulic conditions are such that the coolant can effectively extract heat from the fuel. The fuel does not start heating up until the blowdown is complete, and as the fuel temperatures begin to rise, the temperature profile is effectively flat. This is consistent with fuel temperatures calculated in the literature (Terrani et al., 2014) and is related to the characteristic time for heat produced in the UO_2 to be removed. Effectively, in the absence of fission, decay heat can be removed from the system, assuming a constant heat sink, within 5–10 s. This changes

Table 5
Full core estimation of HBFF susceptibility.

Assumed segment length (cm)	PCT = 656 °C (kg)	PCT = 821 °C (kg)
10.16+	43.3	74.4
15.24+	65	111.6
24.94*	106.4	182.6

+NRC LOCA experimental susceptibility region
*BISON predicted susceptibility region

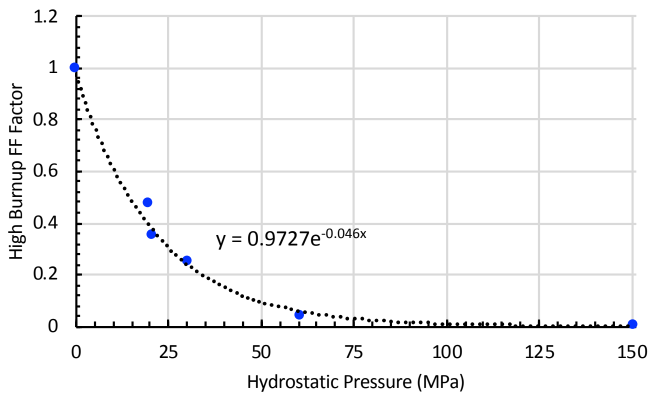


Fig. 33. Relationship between HBFF and hydrostatic constraint normalized from Ref (Turnbull et al., 2015).

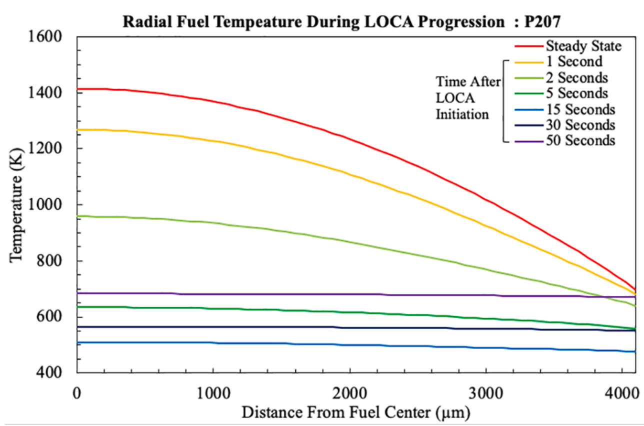


Fig. 34. Radial temperature evolution of prior to and during LOCA.

slightly with increasing fuel burnup; so, the fuel is expected to have a flat temperature profile during the LOCA heat-up phase, regardless of the fuel burnup.

Fig. 35 indicates that a LOCA has adverse effects on the fuel pellet stress state. As discussed above, the radial stress profile under steady-state operation is in equilibrium. Stress relaxation in the fuel occurs through a combination of plastic deformation (i.e., creep, plastic flow, hot pressing) occurring in the center of the pellet and crack formation on the pellet periphery. During a LOCA, the temperature profiles of the fuel pellet changes dramatically, causing stresses to form and become more severe as the LOCA progresses. BISON LOCA results indicate that the center of the pellet experiences significantly high tensile stresses above the unirradiated UO_2 fracture stress (~120–200 MPa); the pellet periphery experiences excessively high compressive stresses in the hoop and axial direction. The stress evolution observed in Fig. 35 are directly related to the equilibrium stresses formed under normal operation. Equilibrium stress form under steady state conditions as a result of the temperature gradient generated during the fission process. Once the gradient is removed, the fuel begins to rapidly contract (primarily in the center of the fuel). This results in tensile stresses forming in the center of the pellet and compression stresses forming on the pellet periphery. In essence, this process is the opposite effect of what is observed during a reactor startup where tensile stresses are formed on the pellet periphery and compression stresses are formed in the center of the pellet. These results indicate that the center of the pellet would form tensile cracks in the radial and circumferential direction, while the pellet periphery may experience compressive failure. However, these results do not indicate whether the stresses would persist following crack formation; nor do they indicate how many cracks are needed to alleviate the stress.

BISON results clearly indicate the relationship between pretransient operating conditions and transient stress formation. Pretransient operating conditions define the equilibrium state of the fuel, and that equilibrium state is contingent on the temperature gradient. As fuel conditions diverge from equilibrium, stresses naturally form until relieved by some mechanism. However, mechanisms governing stress relaxation in UO_2 only occur in fission environments and at high temperatures of > 700 °C. Once power is removed and fuel temperatures decrease below ~ 700 °C, creep (irradiation and thermal) is inactive, so the fuel stress state will remain unchanged, irrespective of time. Pellet cracking has been observed in previously irradiated fuel and is expected. However, these results suggest that additional, potentially extensive cracking above that previously observed is expected. The implication of these results clearly indicates that LOCA transients can be replicated by knowing the rodlet's pretransient operating conditions (i.e., rodlet power history) and uniformly heating the rodlet under representative heating rates with an electrical furnace or nuclear heating systems. Effectively, fuel conditions are set post irradiation, and if the temperature profile is flat and heating rates are prototypic, then the heating method is irrelevant. To confirm, two rodlets with similar pretransient operating conditions and average burnups would need to be identified: one rodlet would be sent to Oak Ridge National Laboratory's Severe Accident Test facility, and the other to INL TREAT for LOCA testing.

5. Conclusions

Results from a full-core LOCA analysis for a core containing high burnup fuel are described herein. VERA was used to deplete a 24-month equilibrium high burnup core design. RELAP5-3D performed a transient thermal hydraulic evaluation of a select few high-impact high burnup fuel rods, and BISON evaluated the steady-state and transient fuel rod response to determine fuel susceptibility to pulverization. The objective of this work was to investigate fuel susceptibility, as well as conditions that lead to fuel susceptibility, to fuel fragmentation, relocation, and dispersal for a realistic case. A secondary objective was to identify prototypic LOCA conditions in order to support accelerated integral LOCA testing.

VERA results identified the location and operating conditions of high burnup fuel rods throughout the core. These conditions were found to be in line with high burnup depletion analyses. Best-estimate thermal hydraulic analyses indicated that high burnup fuel would experience PCTs of ~ 656 °C. Additionally, thermal hydraulic analysis was performed on a once-burned fuel rod, with the highest EOC LHR serving as a bounding condition. The thermal hydraulic analysis results were consistent with the literature. BISON evaluated fuel rod performance prior to and during a LOCA. These results were used to calculate the total amount of fuel susceptible to pulverization throughout the core. The analysis was performed for best estimate as well as for the highest temperature observed in the core. Best estimate BISON results coupled with RELAP TH results identified cladding burst in full length commercial fuel rods are likely to occur at higher temperatures than burst conditions observed in twelve-inch cladding burst test. BISON did not predict cladding burst in high burnup fuel rods because the temperatures where not high enough to induce burst. Furthermore, when the cladding temperature did reach rodlet burst conditions the cladding deformed resulting in large changes to the void volume (i.e., dramatically decreasing rod internal pressures). This effetely resulted in burst occurring at ~ 800C. However, there is additional work that needs to be done to confirm, but the BISON results suggest the probability for burst to occur in high burnup fuel rods is very low. As a result, cladding burst results for the highest power fuel rod was used to calculate the size of the cladding balloon. BISON results indicated that 106 kg of fuel will be susceptible to pulverization, while experimental cladding balloon data suggest 43–65 kg of fuel will pulverize. In an attempt to quantify the worst-case scenario, BISON evaluated pulverization susceptibility in high burnup fuel rods using the RELAP cladding temperature for the highest power fuel rod in the core.

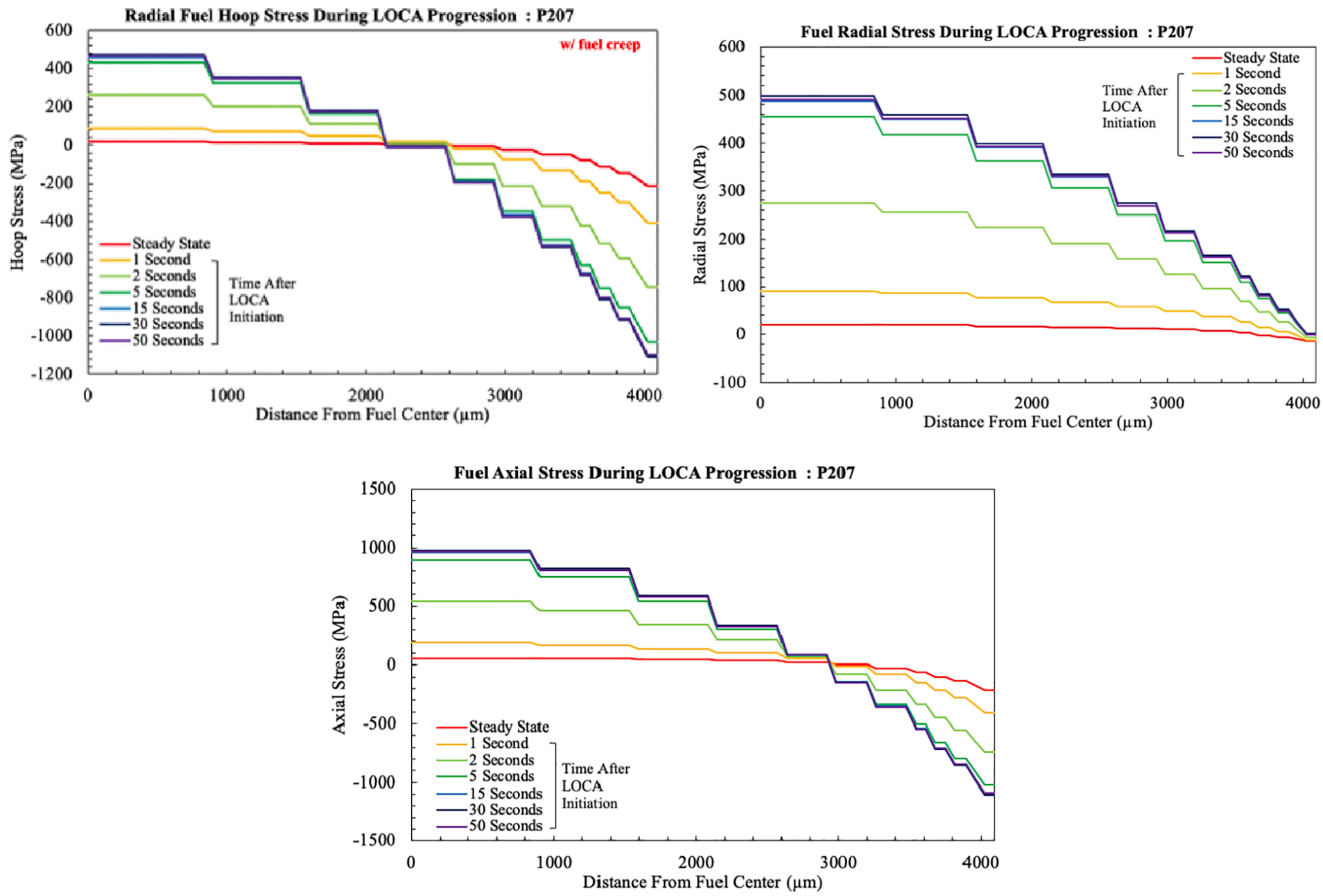


Fig. 35. Radial (a) hoop stress, (b) radial stress, and (c) axial stress evolution **prior to and during LOCA.**

The results indicated 182 kg of fuel would pulverize.

Fuel pellet temperature and stress conditions were calculated prior to the LOCA transient and during the transient. BISON indicated fuel temperatures during the blowdown phase decrease to the coolant temperature and are flat. Fuel temperatures during the refill phase increase uniformly at rate consistent with the cladding thermal boundary conditions; decay heat had negligible impact on fuel temperatures. This is due to the differences between hot full power (HFP) and LOCA thermal hydraulic boundary conditions. At hot full power conditions, fission is generating heat in the fuel pellet and being transported to a (for all intents and purposes) constant thermal boundary conditions. During a LOCA, the heat source is effectively reversed. Decay heat is being generated in the fuel (~7% of the HFP conditions), however, the heat from the other ~ 40,000 rods drive the thermal boundary conditions. Basically, the fuel is being heated from the outside and the decay heat generated by the single fuel rod is being drowned out by the decay heat from the other fuel rods. Fuel stress conditions across the pellet prior to the LOCA range from ±50 MPa. These stress conditions are developed in the presence of a temperature gradient and are in equilibrium prior to the transient. Reactor shutdown removes this temperature gradient resulting in stresses forming across the pellet radius. In the absence of fission and below ~ 700 °C, stress relaxation would not occur, and therefore, the resultant stresses induced by the reactor shutdown, or scram, would not change, irrespective of time after shutdown, unless pellet cracking occurred. At the onset of LOCA, fuel stresses continue to diverge from the equilibrium stress conditions as the flat temperature profile increases to the point where tensile and compressive cracks are expected to form in the center of the pellet and on the pellet periphery. This behavior indicates UO_2 performance under LOCA conditions is reliant on pre-transient operating conditions conditioning the fuel pellet prior to the LOCA and the impact of decay heat would have no impact on pellet fracture or pulverization.

Ultimately, the purpose of this manuscript is to identify prototypic LOCA test conditions by performing a full core LOCA simulation in order to inform subsequent integral LOCA test. There are two types of LOCA test: nuclear (i.e., TREAT/Halden) and electrical (IR furnace). The first of which uses fission induced to generate the appropriate temperature profile prior to the transient, and during the transient, the appropriate thermal boundary conditions are applied to replicate prototypic conditions. The second simple use external heat sources to heat the rodlet up under prototypic heating rates. Evidence shown in the manuscript clearly show fuel temperature during the LOCA heatup phase will be flat and that stress generated in the fuel pellet are a result of removing the temperature gradient and not the LOCA transient (i.e., heatup process). Therefore, it can be concluded that to reproduce LOCA test conditions, one simply needs to uniformly heat up a rodlet, and in effect, a LOCA test performed at TREAT or in an IR furnace should have the exact same results provided the fuel is the exact same (pre-transient conditions, burnup, etc.) Future work should consider replicating in-pile and furnace experimental conditions using commercial fuel irradiated under similar burnup and pre-transient power conditions to support the BISON conclusions.

Declaration of Competing Interest

The authors declare that they have no known competing financial interests or personal relationships that could have appeared to influence the work reported in this paper.

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