Chapter 5

Related Neutronics Parameters and Fuel Safety Criteria and Margins

Historical perspective

The goal of reactor safety is to ensure that the operation of commercial nuclear power plants does not contribute significantly to individual or societal health risks. Reactor safety is concerned with the prevention of radiation-related damage to the public from the operation of commercial nuclear reactors; fuel operational or design limits are introduced to avoid fuel failures during normal operation, and to mitigate the consequences of accidents in which substantial damage is done to the reactor core.

In most countries dose rate limits are defined for a possible off-site radiological release following such accidents; fuel safety criteria which relate to fuel damage are specified to ensure that these limits are not exceeded.

Fuel safety criteria, with derivative fuel design and operational limits are the focus of this chapter. The current safety criteria for light water reactors, which form the large majority of the existing commercial nuclear power plants in the world, were developed during the late 1960s and early 1970s. An underlying idea in this development process is that the consequences of these postulated events are inversely related to their probability. For the sake of simplicity the postulated events are divided into two categories: anticipated transients (or anticipated operational occurrences, AOOs) and postulated accidents. In general, those events whose probability of occurrence varied from ~1 to 10^{-2}/yr were characterized as anticipated transients, or simply transients, while all other events whose probability was less than 10^{-2}/yr were characterized as (postulated) accidents.

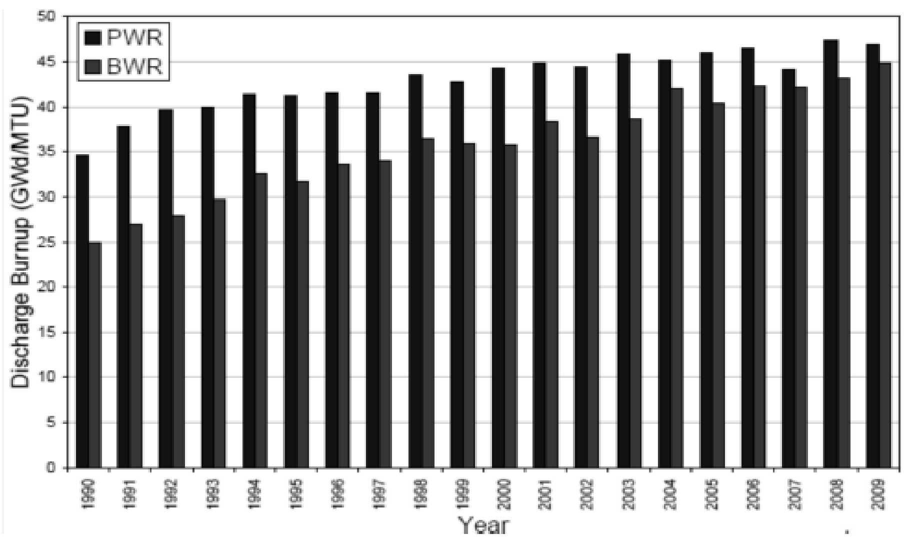
The frequency spectrum within both of these categories varies. Within the transient spectrum are the more frequent events (classified in most countries as inherent to normal operation, or Condition I events), and the less frequent ones (classified in most countries as faults of moderate frequency, or Condition II events). Within the accident spectrum are events that may lead to failure of some fuel rods (e.g. reactor coolant pump seizure, in most countries classified as Condition III events) as well as postulated accidents of low probability (referred to as a design basis accident or DBA, in most countries classified as Condition IV events). Condition IV events include the loss-of-coolant accident (LOCA), and the reactivity initiated accident (RIA), both of which can lead to more substantial fuel failures. The last two DBAs are assumed to have a likelihood or probability of occurrence in the range of 10^{-4} to 10^{-6}/yr.

These probabilities were taken into account in the development of fuel safety criteria. For the more probable transients, as an example, safety criteria allow for no fuel failures. This is usually ensured by no (or only a very small number of) fuel rods in the core to experience the boiling crisis, no fuel melting and no pellet cladding interaction (PCI). With regard to boiling crisis, departure from nucleate boiling (DNB) for the pressurized water reactor (PWR) or critical heat flux (CHF) for the boiling water reactor (BWR) must be avoided during normal operation and anticipated operational occurrences. If fuel failure occurs during normal operation, the number of failed rods is de facto limited by the operational limits on coolant activity. If these limits are exceeded, the operator has to reduce power or shut down the plant. For less probable accidents, the criteria are usually established to ensure core coolability (e.g. limits to the energy deposition in the fuel during a RIA or limits on the temperature and total oxidation of the cladding following a LOCA). Criteria for normal operating conditions were also developed to ensure that the initial fuel conditions prior to a transient or accident do not compromise or lead to exceeding the fuel safety criteria themselves.

During the late 1960s and early 1970s a number of experiments were carried out, which provided information about fuel and reactor core behavior for the more serious design basis accident and beyond design basis accident conditions. This information was used to develop the fuel safety criteria for these accidents as well as the related analytical methods. During the development of these criteria and methods, high burn-up was thought to be around 40 gigawatt-days per metric ton of uranium (GWd/t). Here the fuel burn-up is expressed in terms of gigawatt-days per metric ton of uranium (GWd/tU) or gigawatt-days per metric ton of heavy metal (GWd/tHM). Both are abbreviated GWd/t and both are equivalent to one megawatt-day per kilogram (MWd/kgU), but not equivalent to MWd/kgUO_2. Another aspect that is not always stated clearly is whether the burn-up is rod average or a more locally expressed (and higher) value. Data up to this burn-up had been included in databases for criteria establishment, and regulatory decisions, and it was believed that some extrapolation to higher burn-up could be made. By the mid-1980s, however, changes in pellet microstructure had been observed from a variety of data at higher burn-up along with increases in the rate of cladding corrosion and hydriding (leading to mechanical properties degradation). It thus became clear that something different was occurring at high burn-up and/or new operating environments and that continued extrapolation of data from the existing low burn-up and traditional operating environment was not appropriate.

Meanwhile regulatory authorities in a number of countries had allowed reactors to operate at exposures higher than those used in the development of the fuel safety criteria discussed earlier; in the United States, for example, the Nuclear Regulatory Commission (NRC) has licensed fuel burn-up in commercial pressurized water reactors up to 62 GWd/t (average exposure of the peak rod). It is interesting to note that the burn-up extension trend is stabilizing, as shown in the following figure.

Figure 1: Average US discharged assembly burn-up trends



Source: EPRI.

In Europe, high burn-up test programs continue to be employed, with fuel rods in lead test assemblies attaining exposures of up to 100 GWd/t mainly to gather data for modeling development and validation. In countries where the fuel cycle is closed (e.g. France), there is no incentive to extend the burn-up beyond the current limits; higher burn-up would degrade the isotopic composition of the reprocessed fuels (MOX and reprocessed uranium) and the fuel cycle economics.

As a result of the worldwide trend to increase fuel burn-up well beyond the level of 40 GWd/t during the last 30 years, and the observations regarding pellet microstructure changes and increased rates of cladding corrosion/hydriding at higher burn-up (note: mechanically, corrosion is not harmful to the cladding, only hydrides in the bulk layer are detrimental), a number of test programs were initiated, both of an experimental and analytical nature, to evaluate the effects of the higher burn-up on fuel behavior, especially under RIA and LOCA conditions.

Interest peaked after tests – related to the high burn-up fuel behavior during the reactivity initiated accident – were performed by the French in the CABRI facility and by the Japanese in the NSRR facility. During two specific tests performed with highly irradiated fuel, rods failed and some amount of fuel dispersal was observed at significantly lower enthalpy values than the peak fuel enthalpy limits that had been established earlier or previously approved by the various regulatory authorities. This led to expanded efforts in a number of countries to gain a more complete understanding of highly irradiated fuel behavior under postulated accident conditions.

The Halden LOCA tests with high burn-up fuel indicated that under accident conditions severe fuel fragmentation can take place and the fine grains of fragmented pellets can be released from the damaged fuel rod into the coolant. This phenomenon needs further investigations, but it calls attention to the fact that further increase of fuel burn-up can be limited by LOCA considerations.

In a 1996 report on Nuclear Safety Research in OECD Countries, the Committee on the Safety of Nuclear Installations (CSNI) recommended that “fuel damage limits at high burn-up” be recognized as a safety research area to which priority should be assigned. Specifically, that report indicated “Fuel damage limits should be established for the entire range up to high burn-up. Limits should be based upon appropriate parameters to ensure fuel integrity (i.e. enthalpy or enthalpy rise, DNB, cladding oxidation), and should consider the full range of possible transients, including reactivity insertion and LOCAs”. It was recommended a much broader (than only high burn-up related issues) look at fuel behavior and requirements needed to assure appropriate safety margins of modern fuels and core designs.

Types of criteria

Different types of fuel criteria need to be recognized. Previously several categories of criteria have been identified:

a) Safety criteria – Criteria imposed by the regulator. If preserved, safety criteria ensure that the impact of a DBA on the environment is acceptable.

b) Operational criteria – Criteria specific to the fuel design and provided by the fuel vendor as part of the licensing basis. Operational criteria ensure that safety criteria are not violated.

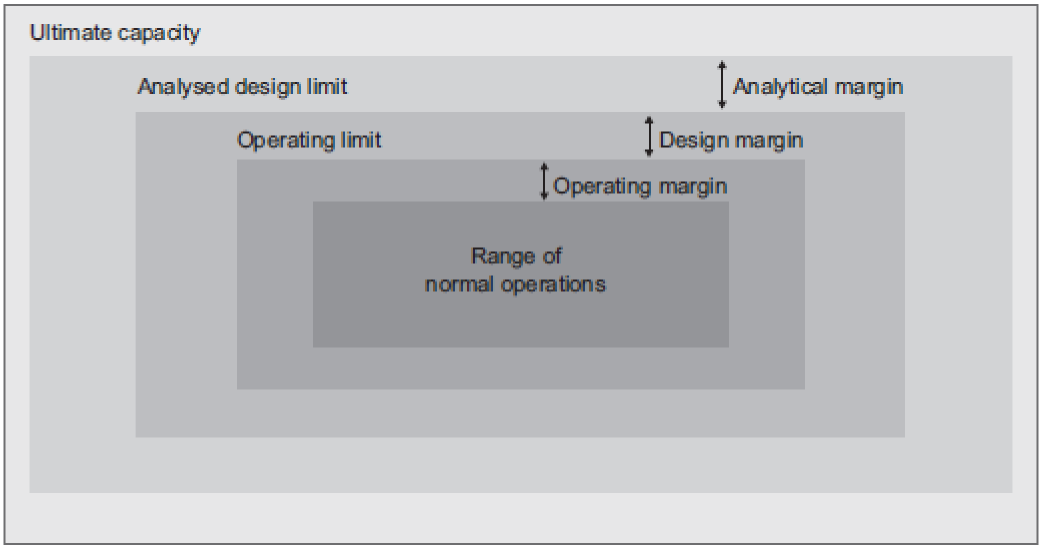
c) Design criteria – Limits employed by vendors and/or utilities for fuel and core design. Design criteria are preserved during the normal operation and anticipated transients.

Unfortunately, the criteria vary between countries. Some criteria, such as fuel rod internal gas pressure limits, have originated with a fuel vendor, and have been subsequently adopted and uniformly applied by the regulatory authorities. Other “safety” criteria, including the previously mentioned rod internal pressure and DNB limits, apply only during normal operation and anticipated transients, and not during accident conditions. During a number of design basis accidents, these criteria are assumed to be violated.

The relative conservatism (or “margin”) between these categories is not always clear. Further, the relative order of the categories is subject to debate. For example, based on the definitions above, the design criteria are most restrictive and the safety criteria the least restrictive.

The Institute for Nuclear Power Operations (INPO) has adopted somewhat different criteria, which are identified as operating limit, design limit and analytical limit (or ultimate capacity). These criteria are schematically shown in the following figure.

Figure 2: Concept of limits and margins



Source: INPO.

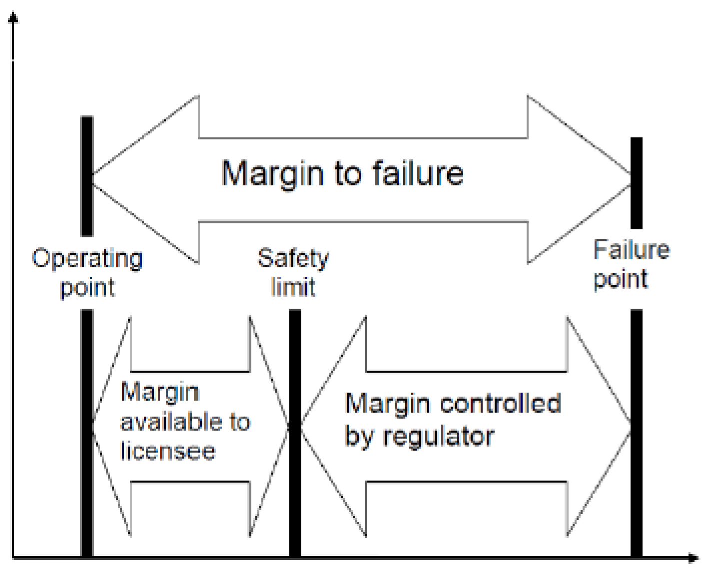
Presumably, the "analytical" margin in this figure is analogous to the "safety" margin. The OECD Nuclear Energy Agency (NEA) has conducted significant work on the issue of margins and limits. Margin is defined as the "difference" (conservatism) between the actual state (load) and damage state (strength). Since both load and strength involve uncertainties or probability distributions, the safety margin is examined in the context of risk assessment. The following figure shows the probability densities for both load and strength, and the relationship between the two.

Figure 3: The relationship between load and strength in terms of margin

In this case, the allowed load is smaller than the assumed strength, and the difference between the two is the margin. Quantifying the total margin (to say nothing of the design margin) is difficult. Even under experimental conditions, it is not so easy to precisely define load and strength (e.g. whether to use tensile strength or compressive strength, and how the load should be applied). It is even more difficult to establish single lines (limits) to represent these distributions. Hence, the safety limit may be simple in concept but more difficult to apply in practice (especially if the limits are defined with a single explicit measurable parameter).

Of special interest is the idea that there are two different types of margin: margin available to the licensee and margin available to the regulator. This is schematically shown in Figure 4.

Figure 4: Types of margin



The previous understanding of criteria - safety, operational or design criteria – is shown in Table 1. In this table, it should be noted that some criteria (DNB/CPR and RIA) appear twice – once in the category of safety criteria, and once in the category of operational criteria. Hydrogen and hydriding appear in both safety and design criteria categories. Other technical issues, such as fuel melting, do not appear at all. This makes it difficult to directly apply either the categories or the criteria identified previously on fuel safety criteria.

The concept of safety, operational and design criteria is the correct one. Recently the technical issues have been placed in a single general category of fuel (safety) criteria along with some other considerations of interest.

Other changes have been made also on fuel safety criteria. For example, the related concepts of departure from nucleate boiling (DNB) and critical power ratio (CPR) have been combined into a single element called critical heat flux (CHF). In summary, the following 23 fuel criteria are now considered, along with a number of “other considerations” (see Table 2):

Table 1. Previous understanding of types of criteria and technical issues

Safety criteria	Operational criteria	Design criteria
DNB/CPR safety limit	DNB/CPR operating limit	Crud deposition
Reactivity coefficients	LHGR limit	Stress/strain/fatigue
Shutdown margin	PCI	Oxidation/hydriding
Enrichment	Coolant activity	Hydride concentration
Internal gas pressure	Gap activity	Transport loads
PCMI	Source term	FA fretting wear
RIA fragmentation	Control rod drop time	Clad diameter increase
Non-LOCA runaway oxidation	RIA fuel failure limit	Cladding elongation
LOCA-PCT		Radial peaking factor
LOCA-oxidation		3D peaking factor
LOCA-H release		
LOCA-long-term cooling		
Seismic loads		
Holddown force		
Criticality		
Burn-up		

Table 2. Current Fuel (safety) criteria and other considerations

Fuel (safety) criteria	Other considerations
Critical heat flux	Core management
Reactivity coefficients	Mixed-oxide fuel
Criticality and shutdown margin	Mixed assembly cores
Fuel enrichment	Slow or incomplete control rod insertion
CRUD deposition	Axial offset anomaly
Stress/strain/fatigue	Cladding diameter increase
Oxidation and hydriding	Cladding elongation
Rod internal gas pressure	Radial peaking factors
Thermal mechanical loads and PCMI	3D peaking factors
Pellet cladding interaction (PCI)	Cladding stability
Fuel melting	
LHGR limits	
RIA cladding failure	
Fuel fragmentation	
Non-LOCA cladding embrittlement/temperature	
LOCA cladding embrittlement	
Blowdown/seismic/transportation loads	
Assembly holddown force	
Fretting wear	
Coolant activity	
Fuel gap activity	
Source term	
Burn-up	

The last safety criteria of the list (burn-up) has a different status than the others: burn-up is, indeed, legally limited in most countries but it is not a safety criteria per se. As a matter of fact, burn-up effect is (more or less) embedded in almost all the other safety criteria and thus accounted for, through the appropriate models included in the calculation codes used to design the fuel and through the burn-up adapted safety limits (e.g. new RIA and LOCA limits for high burn-up fuels).

This list may be amended and supplemented. Whether an individual criterion applies during normal operation, anticipated transients, or accident conditions is noted in the text. Regarding relative conservatism of each category, margins may be set differently in different countries, and will thus depend on the technical and regulatory interpretation of the criteria.

Radial power profile

The most important neutronics parameters related to fuel performance such as power distribution and burnup distribution are usually calculated in the most of the fuel performance codes only in radial direction. In axial direction the fuel rods are considered infinite and this dimension is not treated in solving neutronics and burnup equations. Usually the total power per unit length or volume (such local power density, volumetric heat generation rate or linear heat generation rate) are provided as input along with axial power distribution in the form of axial power factors to particular axial nodes. No treatment is provided to azimuthal dependence of power and temperatures since the current fuel performance codes are usually applied to single fuel rod simulations thus neglecting the real rod environment in a reactor core.

The radial power profile within a fuel pellet is a function of fuel type, reactor type, and burnup. The following model has been developed to calculate the radial power profile in UO_2 and MOX under LWR and heavy-water reactor (HWR) conditions as a function of burnup. The neutron flux distribution (r) within the fuel pellet is described usually by the solution of one-group, one-dimensional diffusion theory applied to cylindrical fuel:

(4.1)

$$\phi(r) = CI_0(\kappa r)$$

for solid pellets, and

$$\phi(r) = C\left(I_0(\kappa r) + \left[\frac{I_1(\kappa r_0)}{K_1(\kappa r_0)} \right] K_0(\kappa r) \right)$$

for annular pellets

where

$$\kappa = \sqrt{\Sigma_a / D}\,,\ \Sigma_a = \sum_k \sigma_{a,i} \overline{N}_i\,,\ D = \frac{1}{3\Sigma_s} = \frac{1}{3\sigma_s \overline{N}_{tot}}$$

and

I, K	=	modified Bessel functions
C	=	a constant
σ_a, σ_s	=	absorption and scattering cross sections
$\overline{N}$	=	pellet-average atom concentration
r_0	=	the pellet outer radius
i	=	subscript indicating all U and Pu isotopes

The evolution of average uranium and plutonium isotope concentrations in the fuel through time can be described as a coupled set of differential equations, which are coupled because the loss of Increases rod internal pressure one isotope by neutron capture leads in some cases to some production of the next higher isotope. These equations are summarized as follows:

(4.2)

$$\frac{d\overline{N}_{235}}{dt} = -\sigma_{a,235}\overline{N}_{235}\phi$$

$$\frac{d\overline{N}_{238}}{dt} = -\sigma_{a,238}\overline{N}_{238}\phi$$

$$\frac{d\overline{N}_{j}}{dt} = -\sigma_{a,j}\overline{N}_{j}\phi + \sigma_{c,j-1}\overline{N}_{j-1}\phi$$

where

j	=	^{239}Pu, ^{240}Pu, ^{241}Pu, and ^{242}Pu
σ_a, σ_c	=	absorption and capture cross sections

Because, the linear heat generation rate (LHGR) and time step duration are input values, the burnup increment for the time step is prescribed and can be related to the flux, the fission cross sections, and the concentrations of fissile isotopes. Thus, flux-time increment, *dt*, can be replaced by the burnup increment, *dbu*, via the relation

(4.3)

$$dbu = \frac{q'''dt}{\rho_{fuel}} = \frac{\alpha}{\rho_{fuel}}\sum_{k}\sigma_{f,k}\overline{N}_{k}\phi dt$$

where

q'''	=	volumetric heat generation rate
ρ_{fuel}	=	fuel density
σ_f	=	fission cross section
α	=	a conversion constant

Furthermore, the distribution of plutonium production is described by an empirical function, f(r), the parameters for which are to be selected based on code-data comparisons on plutonium concentrations as a function of burnup. Thus, the equations for isotope distribution N(r) become:

(4.4)

$$\frac{dN_{235}(r)}{dbu} = -\sigma_{a,235} N_{235}(r) A$$

$$\frac{dN_{238}(r)}{dbu} = -\sigma_{a,238} \overline{N}_{238} f(r) A$$

$$\frac{dN_{239}(r)}{dbu} = -\sigma_{a,239} N_{239}(r) A + \sigma_{c,238} \overline{N}_{238} A$$

$$\frac{dN_j(r)}{dbu} = -\sigma_{a,j} N_j(r) A + \sigma_{c,j-1} N_{j-1} A$$

where, in this case, j = ^{240}Pu, ^{241}Pu, and ^{242}Pu,

$$A = 0.8815 \frac{\rho_{fuel}}{\alpha \sum_i \sigma_{f,i} \overline{N}_i}$$

$$f(r) = 1 + p_1 \exp\left(-p_2 (r_{out} - r)^{p_3}\right)$$

and p_1, p_2, and p_3 are empirically determined constants.

p_1 = 3.45 (for LWR), p_1 = 2.21 (for HWR)
p_2 = 3.0 (for LWR and HWR)
p_3 = 0.45 (for LWR and HWR)

The function $f(r)$ is constrained to have a volume-averaged value of 1.0.

The fission and capture cross sections are different for LWR conditions and HWR conditions due to the difference in neutron spectrum in these reactors. The fission and capture cross sections (σ_f and σ_c, respectively) used in FRAPCON-3 are listed in the Table 3. The absorption cross section (σ_a) is the sum of the fission cross section and the capture cross section. The local power density, q‴(r), which is needed for the thermal analysis, is proportional to the neutron flux and the macroscopic cross section for fission as shown in Equation (4.5).

Table 3. Cross-section values

Isotope	LWR		HWR	
	σ_f (barns)	σ_c (barns)	σ_f (barns)	σ_c (barns)
^{235}U	41.5	9.7	107.9	22.3
^{238}U	0.00	0.78	0.00	1.16
^{239}Pu	105	58.6	239.18	125.36
^{240}Pu	0.584	100	0.304	127.26
^{241}Pu	120	50	296.95	122.41
^{242}Pu	0.458	80	0.191	91.30

(4.5)

$$q'''(r) \propto \sum_j \sigma_{f,j} N_j \phi$$

where

j = ^{235}U, ^{238}U, ^{239}Pu, ^{240}Pu, ^{241}Pu, and ^{242}Pu

The Equation (4.5) can be used to obtain a normalized radial power profile across the pellet. This normalized radial power profile is used as Q(x) in Equation(4.6). At the end of each time step, the isotope concentrations are updated based on the burnup increment, using the above equations. These equations are solved and the concentrations evaluated at every input radial boundary. Because the flux and plutonium deposition distribution functions are prescribed, and the solutions are carried out at ring boundaries, the solution is independent of the radial nodalization scheme; it is also quite stable with respect to time-step size, within the limits dictated by other processes, such as cladding creep and fission gas release.

(4.6)

The steady-state integral form of the heat conduction equation is

$$\iint_S k(T,\bar{x})\vec{\nabla}T(\bar{x}) \bullet \vec{n}ds = \iiint_V S(\bar{x})dV$$

where

k = thermal conductivity (W/m-K)
s = surface of the control volume (m^2)
$\vec{n}$ = the surface normal unit vector
S = internal heat source (W/m^3)
T = temperature (K)
V = control volume (m^3)
$\bar{x}$ = the space coordinates (m)

$$S(x) = P_f P Q(x) \tag{4.7}$$

where

P_f = the axial power factor that relates P to a particular axial node
P = the power function derived from the linear heat generation rate

The calculated radial burnup and power profiles in PWR rod at 25 GWD/MTU burnup is shown in Figure 5.

The volumetric heat generation (W/m^3) at any point within a fuel pellet is proportional to the effective thermal neutron flux multiplied by the sum of the fissile isotope concentrations times their effective fission cross sections. At BOL, the typically low enrichment of the ^{235}U isotope (3 to 5%) constitutes the only fissile isotope, and the radial distribution of ^{235}U is uniform. The thermal neutron flux level is only slightly depressed in the center relative to the edge, resulting in a nearly uniform radial distribution of volumetric heat generation. As burnup proceeds, fission consumes ^{235}U, but ^{238}U captures resonance energy neutrons, resulting in a limited buildup of plutonium, including the fissile plutonium isotopes ^{239}Pu and ^{241}Pu. Because of the large value of the capture cross section at resonance energies, this plutonium buildup occurs preferentially, but not exclusively, at the pellet edge.

The plutonium content in the pellet builds asymptotically towards approximately 1% pellet average and 3 to 4% in the pellet rim. Thus the fissile plutonium concentration at the rim begins to significantly exceed that in the remainder of the pellet as burnup accumulates, and the radial distribution of the volumetric heat generation becomes progressively edge-peaked. The radial distribution of fuel burnup (in relation to the initial concentration of heavy metal atoms) also becomes progressively edge-peaked.

Accurate calculations of the evolution of neutron flux distribution and fissile isotope concentrations within a fuel rod require detailed neutronics codes that account for all the interactions and the specific time-dependent neutronic environment of the rod. However, the environments and fuel designs in standard LWR cores are sufficiently similar to permit approximate, one-dimensional one-group calculations, using effective values for fission and capture cross sections.

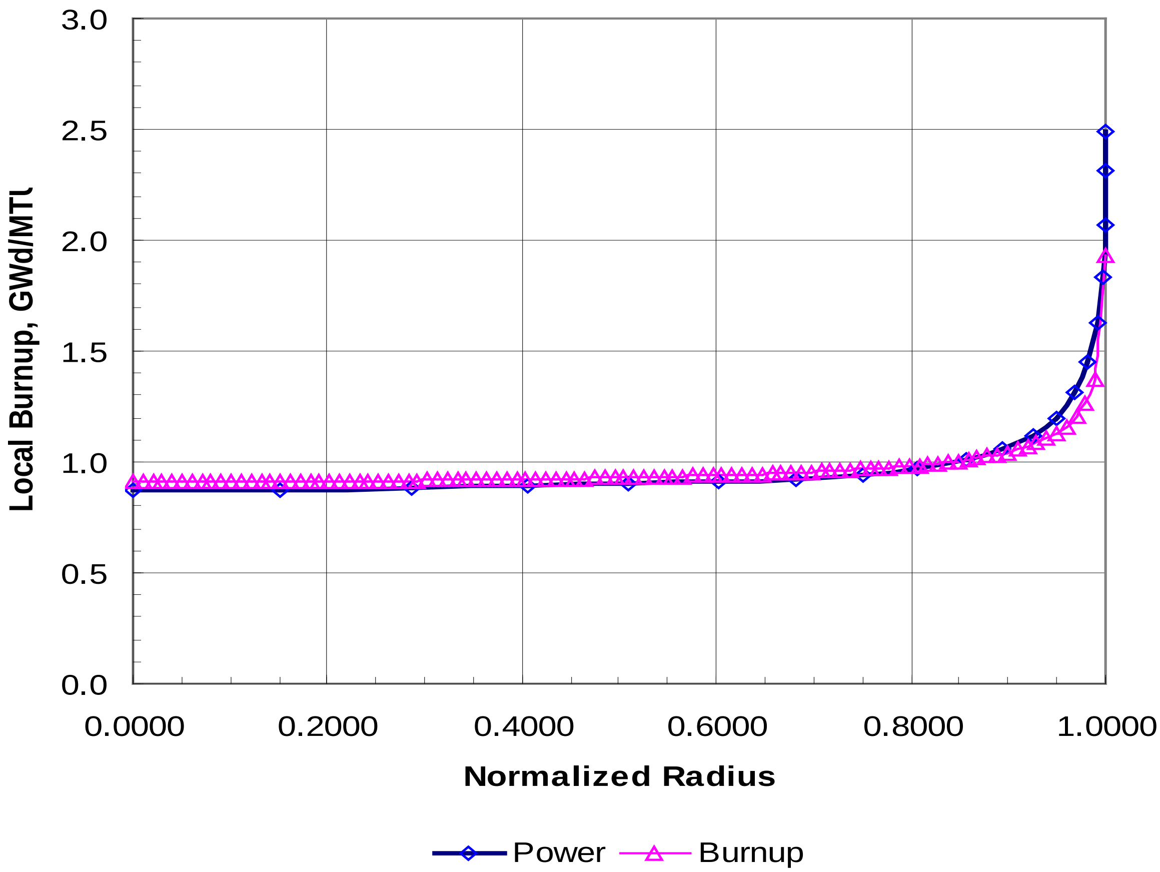


Figure 5. Radial Power and Burnup Profiles (PWR Rod at ~25 GWd/MTU)

The Figures 6 and 7 show an example of the plutonium buildup and its consequences, which apply to a C-E 14 × 14 type fuel rod with 4.5% ^{235}U enrichment. The Figure 6 shows the radial distributions of heat generation and burnup at 60 GWd/MTU pellet-average burnup. The Figure 7 shows the ratios of the edge (outermost 1%) to volume-average values for heat generation and for burnup. The edge-peaking becomes significant from 20 GWd/MTU burnup onward. Electron microprobe scans across transverse cross sections cut from irradiated fuel pellets reveal the relative radial distribution of selected fission products. If the selected fission product is extremely stable (i.e., stays fixed at the position of creation throughout the irradiation), then its relative radial distribution indicates the radial distribution of the fuel burnup. This is only indirect evidence for the radial distribution of the heat generation, which of course is changing throughout the irradiation.

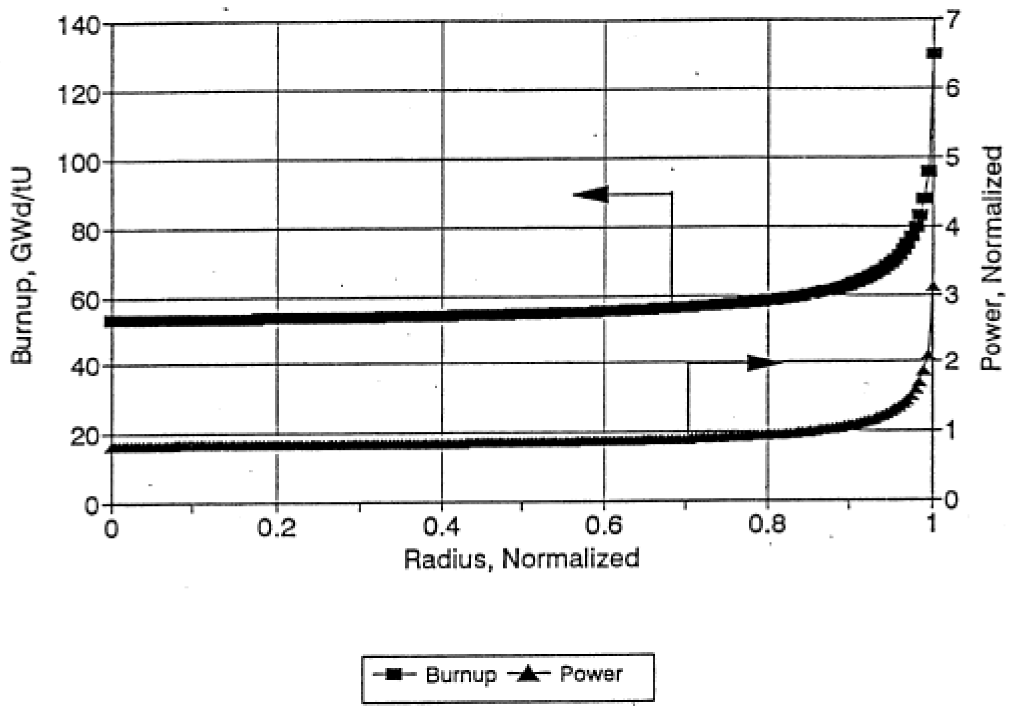


Figure 6. Radial power and burnup distribution at 60 GWd/MTU

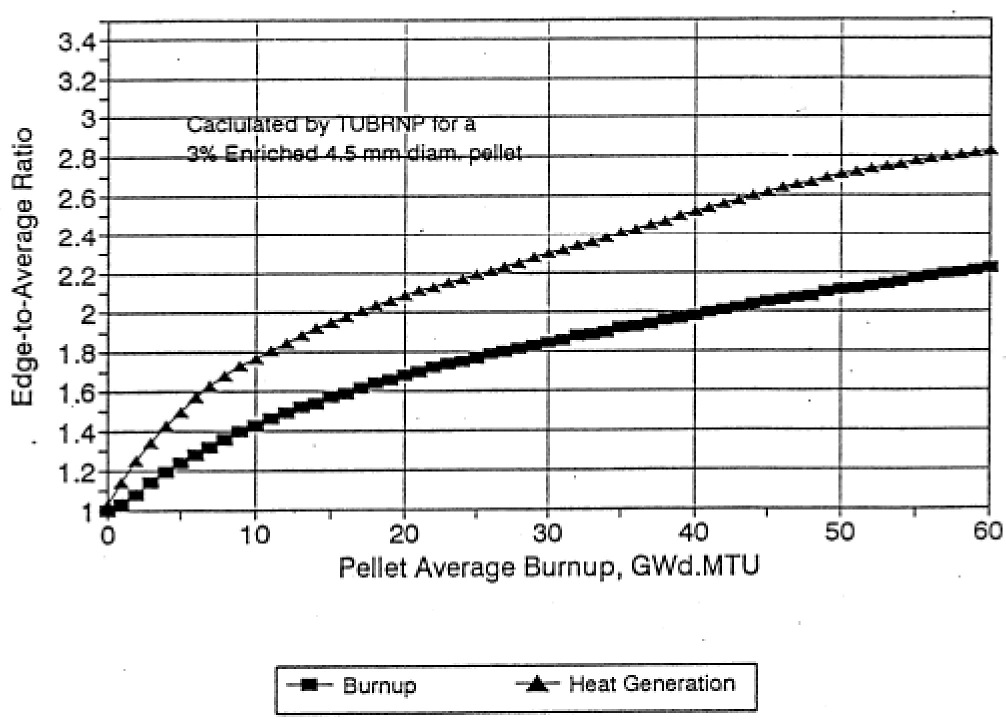


Figure 7. Edge-to-average value for burnup and heat generation as function pellet average burnup

However, the comparative radial scans on burnup indicators at different burnup levels for the similar fuel types, subjected to similar irradiation histories, give detailed evidence about the evolution of the radial power distribution. The fission product neodymium, a rare-earth metal, has been proven to be extremely stable chemically and thermally within the fuel; it is therefore a reliable burnup distribution indicator.

In the High-Burnup Effects Program, electron microprobe scans for neodymium were performed on uranium LWR test fuel and uranium pellets from BWR rods, with pellet burnups ranging from 25 to 55 GWd/MTU. The Table 4 lists the pertinent parameters for these rods (pellet dimensions, enrichment, and burnup). The Figure 8 show HBEP neodymium distributions measured by electron probe micro analysis (EPMA), together with the radial burnup distribution predicted by computer code model. To make this comparison on radial distribution, the neodymium concentrations have been transformed to burnups, and the predicted and measured values have been forced to have the same volume-averaged value.

Table 4. Parameters of rods with measured data

Rod Number	Pellet Inner/Outer Radii, mm	U-235 Enrichment, %	EOL Burnup, GWd/MTU	Reactor Name and Type
A3/6-4	5.22/0.0	3.08	55	TV0-1 BWR (1.5-m Elevation)
H8/36-4	4.97/0.0	1.39	55	TVO-1 BWR (2.2-m Elevation)
BK-365	4.09/1.27	6.97	83	BR-3 PWR Test Reactor
D200	4.52/0.0	3.2	25	Obrigheim PWR
D226	4.56/0.0	3.2	45	Obrigheim PWR

These code-data comparisons are excellent regarding the general shape of the radial burnup distribution at various burnup levels. The predicted value of the relative burnup at the surface of the pellet consistently exceeds the measurements near the surface; however, it is very difficult in this steeply changing region to position the EPMA analysis area (the 2- to 10-µm-diameter "spot") at the exact surface of the fuel without overlapping into the gap region that contains no fuel and hence biases the measurement downward. Therefore, because the predicted shape of the distribution does follow the measurements closely out to very nearly the pellet surface, the predicted surface values are considered realistic.

The revised fuel pellet radial power distribution model has the following ranges of application:

a) Burnup: 0 to 80 GWd/MTU;
b) Temperature: 300 to 3000K;
c) Fuel types: Uranium and uranium-plutonium sintered pellets (but not uranium-gadolinium pellets).

The uncertainty on the localized (ring) burnup (one standard deviation) varies with the position in the pellet and with pellet burnup. Based on statistical analysis of the code-data comparisons presented herein, the Table 5 provides estimated uncertainties for localized burnup and power (one standard deviation) for pellets with average burnups greater than 25 GWd/MTU. It should be noted that these uncertainties apply to UO2 fuel only, and the code has not been validated against uranium-plutonium fuel and uncertainties, which are unknown.

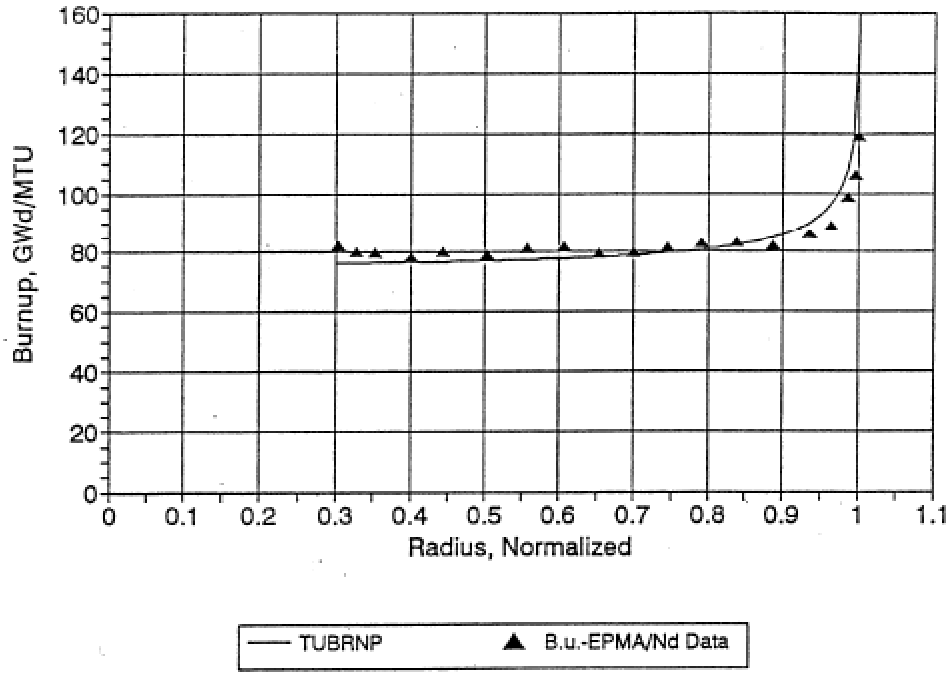


Figure 8. Comparison of calculated and measured data for radial burnup distribution

Table 5. Estimated uncertainties for localized burnup and power

Position within the Pellet (normalized radius where 1.0 is outer surface)	Relative Uncertainty on Burnup (1 standard deviation), %	Relative Uncertainty on Heat Generation, %
0.0 to 0.9	7	8
0.9 to 0.98	12	15
0.98 to 1.0	50	50

Rim structure formation

Since the relatively recent realization of the importance of rim structure formation, usually referred to as the “rim effect”, considerable efforts have been made to arrive at an empirical as well as phenomenological and mechanistic understanding. The present understanding cannot be considered complete, and further work will be needed at the phenomenological level, especially for very high burn-ups.

Rim structure formation has important influences on thermal, fission gas release and PCI behavior under normal operation and transient conditions. The formation of high burn-up structure (HBS, also known as "rim structure formation" or "rim effect") occurs in nuclear fuel materials as a consequence of excessive fission damage and in-growth of fission products. HBS is also referred to as rim structure, as it is first formed near the outer surface (rim) of UO_2 fuel pellets where the burn-up is highest because of resonance neutron capture by ^{238}U forming fissile ^{239}Pu. It can also be observed in Pu-rich agglomerates of MOX fuel. The HBS is fully developed at about 70 GWd/t local burn-up and is characterized by sub-micrometer-size grains and large micrometer-sized pores.

Rim structure formation starts at the pellet periphery where the number of fissions is highest due to the build-up of plutonium. The same structure has also been generated in highly-rated UO_2 fuel disks kept at sufficiently low temperature and in plutonium-rich agglomerates of MOX fuels, showing that the number of fissions is the key parameter for this phenomenon to develop. Fully developed HBS is characterized by a loss of optically definable grains, i.e. the development of sub-micrometer-size grains.

Electron probe micro analysis (EPMA) shows that the matrix is depleted of fission product xenon (Xe) with a concurrent formation of high concentration of pores with a volume fraction of 10-15%, depending on burn-up. X-ray fluorescence (XRF), micro-coring and secondary ion mass spectroscopy (SIMS) show that Xe is retained in the pores.

Apart from burn-up, several parameters play a role in the HBS formation and may influence the threshold value at which HBS starts to develop: HBS formation occurs only at sufficiently low temperatures (< 900-1 000C), as demonstrated by restructuring of Pu-rich agglomerates of MOX fuels and with disk fuel irradiation at different temperatures.

There are some indications that the threshold burn-up might be increased by increasing grain size and higher hydrostatic stresses. A similar effect is likely to be induced by higher temperature and lower fission density, as this phenomenon is related to a competition between creation and recovery of point defects as well as to fission gas super-saturation in the matrix followed by fission gas bubble formation at low temperature.

This is clearly a field where further research and development will be necessary in order to precisely determine the influence of the different parameters. For burn-ups approaching 100 GWd/t, a much larger volume will be affected by HBS formation. Late in life, high burn-up fuel will be operated at low power and thus low temperature (< 1 000C), in which case the entire pellet cross-section may, in principle, eventually be affected by HBS formation. While fuel behavior during normal operation (swelling, fission gas release, failed fuel) may be affected, the overriding impact of HBS formation is expected to be on off-normal behavior (e.g. fuel relocation, fission gas release and dispersal during LOCA/RIA).

Changes in fuel design and operation

The previous fuel safety criteria were developed in the late 1960s to early 1970s and were based on tests and related analyses with the then utilized fuel and core designs, fuel and cladding materials (originally Zircaloy-2 for BWRs and Zircaloy-4 for PWRs), and burn-up levels not exceeding 40 GWd/t. In order to optimize fuel cycle cost, the nuclear industry began work in the mid-1980s on new fuel and core designs with the aim of increasing the fuel burn-up, by extending the number of cycles or the cycle length or upgrading the power level. This again leads to a number of basic design changes, for example new cladding alloys.

Fuel design should be in concordance with the general design criteria that governs the design and operation of nuclear power stations including fuels. Thus, the existing fuel criteria should be examined against the general design criteria, as applicable. High fuel burn-up is of great interest to some utilities due to their need for reducing fuel cycle cost, and may be enhanced by electric power deregulation. Thus, high burn-up capability is very important in the design of fuel, and this has triggered activities worldwide.

To achieve high discharge exposures and gain thermal margins, more advanced fuel designs were introduced. The fuel pin geometry changed from coarse pins with large fuel cladding diameters to slimmer pins with smaller fuel and cladding diameters thus reducing the heat flux per cladding surface area. The number of rods per assembly has increased in both BWR and PWR applications. The cladding wall thickness also has been reduced and lies today in the range of 700 to 600 μm (or even lower). Additional mixing devices were introduced to gain more margin.

In parallel with these dimensional changes, the cladding materials for light water reactor (LWR) fuel have also undergone significant evolutions. To reduce the corrosion rate and hydrogen uptake in the base zirconium metal, new alloys for PWR cladding were introduced. There is little evidence that niobium affects corrosion and hydrogen pickup. However, it is known to introduce dimensional stability. Niobium has been used in Russian alloys for years and is not new. In addition to these changes in alloy composition, several inner and outer liner concepts have been introduced to cope with performance problems [e.g. BWR inner liners for stress corrosion cracking/pellet cladding mechanical interaction (SCC-PCMI) resistance, PWR outer liners for corrosion reduction at high power].

Assessing the potential effects of changes

Numerous criteria related to fuel damage are used in fuel design and safety analyses: these fuel criteria may differ from country to country. Some are used to minimize cladding degradation during normal operation. Some are used to maintain cladding integrity during anticipated transients, thus avoiding fission product release. Some are used to limit fuel damage and ensure core coolability during design basis accidents, and some are used to limit the public risk from low probability severe accidents.

It can be difficult to categorize these fuel criteria according to event type. For example, limits are sometimes placed on cladding oxidation during normal operation to ensure good operational performance, while in other instances, such oxidation limits may be linked to cladding mechanical strength/ductility for LOCA performance.

The fuel criteria are therefore listed with only modest attempt to categorize them according to event type or risk significance. The matter of relative importance of these fuel criteria is left to the regulatory agencies and others who utilize this information. For each of the fuel criteria, a brief description of the criterion as it is used in several applications is provided along with the rationale for having such a criterion.

Design changes such as different cladding materials, higher burn-up, and the use of MOX fuels, can affect fuel-related margins and, in some cases, the fuel criteria themselves. In the following paragraphs, some of the more important effects are mentioned in order to indicate whether the criteria need to be reevaluated.

The following discussion may not cover all possible effects, but should be sufficient to identify those criteria that need to be addressed.

Fuel safety criteria

Critical heat flux

Critical heat flux (CHF) or boiling crisis describes a thermal limit where a phase change in the reactor coolant occurs during heating. In a PWR, the CHF occurs when the bubble density from nucleate boiling in the boundary layer of a fuel rod is so great that adjacent bubbles coalesce and form a vapor film on the surface of the rod. Heat transfer across the vapor film is relatively low relative to the liquid, and the occurrence of CHF is accompanied by a marked increase in the cladding surface temperature. Under such conditions, rapid oxidation (or even melting) of the cladding can take place. This may result in cladding failures.

Similarly, in a BWR the (critical) heat flux at the onset of transition boiling must not be exceeded. In the BWR, the CHF is reflected by the critical power ratio (CPR), the ratio of the critical heat flux to the actual heat flux of a fuel rod. In the PWR, the critical heat flux is reflected in the departure from nucleate boiling ratio (DNBR), the ratio of the CHF (the heat flux needed to cause departure from nucleate boiling) to the local heat flux of a fuel rod.

These ratios incorporate margin to the phenomena. CHF correlations are derived from the analysis of experimental data from electrically-heated (unirradiated), large (usually full-scale) fuel bundles or arrays tested under laboratory conditions. The correlations make it possible to determine the critical heat flux over a wide range of test conditions, such as pressure and flow rate. The limiting DNBR is a safety limit defined such that fuel rods will not experience CHF during normal or expected operation (Condition I and II events). This limit is also used to indicate fuel failures for some postulated accidents (Condition III and IV events) in evaluating off-site dose rates. In other postulated accidents, such as the large break LOCA, most rods in the core are expected to exceed CHF and off-site doses are determined by other methods. Since the correlation links the CHF to the test parameters under which it was derived, this correlation is a mathematical fit to test (not operational) data. The fuel supplier performs such tests for every assembly design specifically. Thus, the CHF correlation is fuel assembly type specific.

The correlation parameters include pressure, mass velocity and flow quality; and tests are performed while varying each of these parameters separately. The statistical method to establish the safety limit is sometimes based on a Monte Carlo technique, which calculates the critical heat flux for each assembly at multiple locations and under varying test conditions, while introducing random variations in the input variables based on their known uncertainties. As a consequence, critical heat flux correlations may be considered to properly reflect the modern fuel and core designs; it is one of the few areas where statistical methods are applied consistently, with a rigorous uncertainty treatment. To date, almost no fuel has failed due to inadequacies in establishing these safety limits.

The 1988 dry-out fuel failures in Oskarshamn 2 (Sweden) were caused by excessive channel bow and incorrect core monitoring model input data. A total of four rods operated around 20-30% in excess of the safety limit CPR for several months prior to failure. A remaining issue is that uncertainties in the experimental case do not necessarily represent uncertainties in the reactor core – particularly in local power levels – although that assumption is often made. Typically, DNBR safety limits are around 1.15. From this limit, it is presumed that, with 95% confidence and 95% probability, DNB shall not occur for maximum powered fuel rods under steady-state and AOO conditions. The issue is whether the "maximum powered fuel rods" in the core have been correctly captured. CPR and DNBR limits ensure that only a very small amount of fuel cladding (0.1% of all fuel rods, in most countries: in Germany, DNB shall not occur for the highest rated rods) is statistically (95/95 level) expected to fail during anticipated operational occurrences (AOO), and indicate when fuel failure occurs during postulated accidents so that off-site doses can be estimated.

To also maintain adequate fuel performance margins during normal steady-state operation, additional margin is usually applied to the safety limit CPR/DNB, which corresponds to the heat flux increase during the worst AOO; this constitutes the operating limit that is continuously verified during plant operation. It is unlikely that critical heat flux methodology, the related safety limits, or the methods used to establish these limits, would be subject to significant change. Some testing seems to be needed, including full scale testing to establish the proper thermal-hydraulic modeling of new assembly designs. However, CPR and DNBR correlations are generally developed from data on un-oxidized, or lightly oxidized, fresh cladding tubes and may not be accurate for high burn-up cladding. For a given linear heat generation rate (LHGR), the heat transfer coefficient for rough oxidized rods will be higher compared to smooth un-oxidized rods. That is, CPR/DNBR based on un-oxidized rods is bounding in the absence of other heat transfer effects of the oxide layers.

Fuel rod heat transfer characteristics are likely to be affected by heavy oxide coatings (which sometimes exhibit spallation) that may appear on cladding at high burn-up, or by heavy CRUD layers. CRUD – Chalk River Unidentified Deposits – corrosion products deposited on the fuel rod cladding, first identified at Chalk River Laboratories in Canada. Material and fabrication variations may make small changes in heat transfer characteristics, but the effect of oxidation or CRUD on surface conditions could be an important effect. Thus, the effect of oxidation or CRUD on surface conditions ought to be addressed, but not within CPR or DNB. It has been noted that exceeding critical heat flux, particularly for a short period of time (a few seconds) may not adversely affect the cladding. Historically, the United States has maintained that cladding-to-coolant heat transfer must not exceed critical heat flux.

However, Japan has been conducting research on post-boiling transition fuel integrity and has proposed a standard in their regulations for judging fuel integrity in cases of boiling transition and the reuse of assemblies that have previously undergone transition boiling. The Japanese assert the need for three specific, accurate correlations in order to predict the fuel cladding temperature after boiling transition:

a) the onset time of boiling transition;

b) the heat transfer coefficient between cladding surface and coolant after boiling transition;

c) a correlation to predict rewet time.

Finally, it is noted that significantly higher cladding temperatures due to operation in a film boiling regime may not affect alternative cladding materials (e.g. ceramics). The phenomenon of transition to film boiling may not occur in some environments (e.g. sodium coolant or very high pressure light water reactors where the coolant may be operated beyond the vapor-liquid critical point for water at 22.1 MPa and 374°C). The current CRP and DNBR safety limits are shown in Table 6.

Reactivity coefficients

The concept of reactivity coefficients has been introduced in order to simplify the analytical treatment, e.g. quantifying the feedback reactivity effects in the point kinetic equation and increase our understanding of reactivity changes due to various physical parameters. Reactivity coefficients are thus an analytical matter; in terms of LWR safety criteria, there is a general requirement that either the moderator temperature coefficient (see below) or the total of all reactivity coefficients be negative when the reactor is critical, for providing negative reactivity feedback (or that the effects of any positive reactivity coefficient be inconsequential).

Table 6. Current CRP and DNBR safety limits

Country	Criterion type	Value(s)	Basis	Relation to other criteria	Effect of recent changes	Type of methods and rational
Belgium	CHF/DNB	Depending on the correlation used	95/95, correlations: W3 (for low pressure), WRB-, HTP, ERB-, FC, ABBX-, ...depending on the fuel supplier	DNB operating limit	No change, verification required (new design, mixed cores)	Statistical
Canada	CHF		Correlations (e.g. Balint-Cheng)			
Czech Rep.	DNB	Depending on the correlation and TH models used	95/95 VVER440: variants of Czech correlation of PG type 95/95 VVER 1000: special Russian correlation CRT-1 for bundles with mixing vanes	3-D peaking at VVER440, DNB oper. limit at VVER1000	Verification required	Statistical
Finland	DNB/CPR	1.33/1.06	95/95/<0.1% of rods may experience DNB, correlations		No change (burn-up limit 40 GWd/t)	Statistical
France	CHF	1.17, 1.30	95/95, correlations (WRB-, W-3 for low pressure)	Operat. limits (e.g. axial offset)	No change, verif. required (new design, mixed core)	Statistical
Germany	DNB/CPR	1.15/1.09	95/95-correlations (PWR), <1 rod experience dryout -- THAM method (BWR), all correlations are FA specific	Addit. oper. crit.	Values change dep. on design	Statistical
Hungary	DNB	1.33	95/95, correlations (Bezrukov)	Pin power limit	Values change dep. on design	Statistical
Japan	DNB/CPR	1.17*/1.06*	95/95/<0.1% of rods may experience DNB, correlations (e.g. MIRC-1, NFI-1)	DNB/CPR oper. limit	No effect	Statistical
Korea (Rep. of)	DNB	Depending on the correlation used	95/95, correlations (KCE-1, NGF, WRB-1 etc.)	DNB oper. limit	No change, verif. required	Statistical
Netherlands	DNB	1.30	95/95, correlations (W-3)	DNB oper. limit		Statistical
Spain	DNB/CPR	Various	95/95, correlations provided by fuel vendors	DNB/CPR oper. limit	Values change dep. on design	Statistical
Sweden	DNB/CPR	1.17/1.06	95/95, correlations (VRB-1)<0.1% of rods may experience dryout	DNB/CPR oper. limit	Values change dep. on design	Statistical
Switzerland	DNB/CPR	1.15-1.45/1.09	95/95, correlations	DNB/CPR oper. limit	Values change dep. on design	Statistical
UK	DNB		95/95, correlations	DNB oper. limit		Statistical
USA	DNB	various	95/95, correlations	DNB oper. limit		Statistical

*Not criteria but typical value.

The fuel temperature or Doppler coefficient $d\rho/dT_f$, where ρ is reactivity, responds promptly to the energy deposited in the fuel, whereas the other coefficients are delayed. The fuel time constant (on the order of a few seconds), which depends mainly on the fuel specific heat, conductivity and diameter, affects the time delay of changes in moderator temperature and void fraction. The fuel temperature coefficient therefore depends slightly on the enrichment and the fuel burn-up – the higher the burn-up, the harder the spectrum, so in general the change of the fuel temperature coefficient with burn-up is small in light water reactors.

The strong negative void coefficient in BWRs gives these reactors inherent stabilizing characteristics without operator intervention. In modern fuel designs, water is added in the central part of the bundle by special water channels of various geometries inside the fuel assembly, which is not heated up as much as the coolant water in the rest of the assembly and has a much lower void fraction thus producing a less negative void coefficient.

In PWR under normal operating sequences there is no void in the core. However, in the case of abnormal events like loss of primary coolant or loss of pressure, the coolant may start to boil and void appears and reduces the neutron absorption in boron which results in a positive contribution to the void coefficient (however there is also less moderator available).

At operating temperature when the boron concentration is low, this effect will be small and the void coefficient remains negative. At low temperature when the boron concentration is high, the effect is large and the void coefficient may turn positive. An increase of the moderator/coolant temperature Tm causes mainly two effects:

a) the density of the water decreases and the effect is similar to that of void increase;

b) the thermal neutron spectrum becomes harder and so the effective neutron cross-sections change.

In a PWR with a strongly borated coolant $d\rho/dT_m$ is negative at normal operating conditions but is slightly positive at lower temperatures. Due to the boron concentration decrease at the end of the cycle, the moderator temperature coefficient is becoming also more negative at the end of the cycle.

This has some impact on cooling down accidents such as the steam line break accident because more positive reactivity is introduced from the cooling and the reactor returns to a higher power level than before. The system pressure in a BWR is related to the saturation temperature of the moderator. Depressurization of the system will cause flashing, i.e. production of steam bubbles in the water. Such an event introduces a negative reactivity change in a BWR and does not lead to safety problems as far as reactivity is concerned.

The effect of a positive pressure pulse is only of interest in a BWR, where significant voiding exists. A sudden increase of the system pressure, e.g. one caused by a turbine trip, will result in a partial void collapse leading to a positive reactivity change. In order to have the same cycle length, high fuel burn-up usually implies the loading of more reactive fresh fuel bundles. This additional reactivity is compensated for by fuel (addition of burnable poison) and core design, keeping in mind that the basic safety criterion (either the moderator temperature coefficient or the negative total reactivity coefficient) must be fulfilled.

In summary, although the reactivity coefficients may be affected, the effects of new fuel design changes are not considered to affect the corresponding safety criteria themselves.

Criticality and shutdown margin

Attaining reactor sub-criticality must be assured either by sufficient reactivity worth of control rods and/or sufficient boron concentration in the primary coolant. For control rods, this sub-criticality requirement becomes the so-called shutdown margin (SDM). SDM is defined as the margin to criticality (k_{eff} = 1) in the situation with all control rods inserted and the strongest control rod withdrawn. The SDM should be sufficient for achieving hot zero power; for the BWR, SDM is analyzed at cold zero power with a xenon-free core, for conservatism.

The technical specification limit for SDM, usually of the order 0.3 – 0.5% Δk/k, is mostly established from the assumed envelope of uncertainties in the determination of k_{eff} and the control rod manufacturing tolerances. This limit is usually verified at least during (reload) cycle startup; design limits for SDM are usually 1% Δk/k or higher, to protect against unforeseen systematic biases in the prediction of the k_{eff} value.

The required sub-criticality in shutdown state is given in the operating technical specifications (OTS). The required sub-criticality is met by adjusting the required boron concentration for a given RCCA (Rod cluster control assembly) position (e.g. controls rods inserted, shutdown banks out). The SDM corresponds to the amount of sub-criticality after a trip with the assumption of a stuck rod. The SDM value is taken as the initial sub-criticality for the steam line break accident analysis.

For the PWR, an increase of boron concentration is required to achieve cold shutdown; this is provided by the available boron/volume control systems. Generally, for PWR and BWR, the boron SDM is the margin to criticality (k_{eff} =1) for the situation in which the emergency boron injection system is activated. The (high) boron concentration should be sufficient to assure that the reactor achieves shutdown without control rod movement; for conservatism, no credit is taken for xenon present in the core.

Emergency boron SDM limits are established similar to the above control rod SDM limits, i.e. those based on calculation and system uncertainties. Values for the emergency boron SDM range from 1 to 4% Δk/k, depending on whether the analysis is performed using generic and/or cold reactor conditions or more realistically reflects specific plants/cycles.

Normally, the emergency boron SDM is not explicitly included in plant technical specifications, but is rather verified analytically as part of the safety analysis and reload licensing process. Highly optimized core designs have often shown a decrease in margin to the SDM criteria (usage of higher enrichment levels, often in conjunction with more burnable poison). However, modern fuel designs are also optimized to improve the SDM performance, and may counteract these effects. These fuel enrichment and core design strategies are provoked or enhanced by operating strategies to save fuel cycle cost, which includes high fuel discharge exposures, long fuel cycles and/or thermal power uprates. In the case of MOX fuel, smaller control rod worth and boron worth have also reduced the SDM performance.

These reduced margins have, in some cases, induced plant changes such as:

a) Use of new control rods with higher worth (more/different absorbing material);
b) Use of part-length or limited worth ("gray") control rods will reduce the RCCA worth and will give no benefit for the SDM;

c) Higher number of installed control rods (if plant design permits);
d) Increase of boron system capacity (if possible);
e) Use of enriched boron.

in order to compensate for the lost margin. Ultimately, fuel and core must be designed such that safety criteria are met; these criteria have not been challenged so far. It is judged that the existing SDM criteria themselves are unaffected by the new design changes. However, if realistic or best-estimate modeling is used to establish or analyze these criteria, such models should be well verified; in particular, the associated modeling uncertainty should be quantified in order to assess the margin to safety.

Fuel enrichment

Enrichment limits around 5 wt% U^{235} are used in connection with criticality considerations for fabrication, handling, and transportation. For some high burnup applications, higher enrichments may be needed. To date, the validation of criticality safety codes and associated cross-section libraries for LWR fuel has focused on enrichments of 5 wt% and below.

Neither benchmarks of code performance nor the bases for extrapolating code performance in the enrichment range of 5-10 wt% have been well established. Moving into this range will require care because the physics of criticality begins to change as enrichments reach 6 wt% and beyond, where single moderated assemblies can go critical and criticality of weakly moderated or unmoderated systems becomes possible. Enrichments above 5 wt% will require redesign of some fuel fabrication and handling equipment and fuel transportation packages. The possibility of re-criticality during accidents, in particular in severe accident core melt sequences should also be addressed as this could alter the progression of such accidents.

CRUD deposition

The maximum amount of CRUD deposited on the cladding can be estimated. This is sometimes done as a function of burn-up (and/or power) but at least at the end of the fuel lifetime. The maximum value has to be considered, and the assumed value is verified against data from measurement 5 (e.g. hot cell examinations). The issue of post-irradiation measurement of CRUD thickness is controversial as some CRUD dissolves on shutdown.

Various CRUD levels are being used by vendors, according to the design models and/or the fuel designs themselves. Firm (safety) limits on CRUD deposition are not defined, although the amount of CRUD deposited and its composition can be significant to the corrosion performance of the cladding (example: CRUD induced localized corrosion or CILC). In addition, investigators should consider the various forms of CRUD (tenacious, fluffy), the thermal conductivity of CRUD, the effects on cladding-to-coolant heat transfer, and when it occurs or when it is deposited (during operation/during shutdown).

New design changes, such as cladding materials and their manufacturing processes (e.g. surface finish), may well influence the build-up of CRUD and thereby the corrosion performance of the fuel clad. The CRUD composition could affect the corrosion locally, either by acting as a thermal insulator or by chemically favoring the corrosion process. Also the water chemistry characteristics influence the type and character of the CRUD build-up: as an example, the ratio of 2-valence to 3-valence components in the reactor water (e.g. Zn/Ni or Fe, respectively) could determine the type of CRUD (spinel vs. hematite), thus influencing the corrosion rate in a different manner.

Experience has shown that the most important factor to consider when implementing the chemistry strategies is to address the correlation between CRUD deposition and corrosion kinetics at the same time, because some practices that can be good from one perspective but poor for the other. New fuel and core designs, high burn-up and long fuel cycles are issues that could influence CRUD build-up through associated changes in cladding materials, surface area and power history.

No specific limits are directly imposed regarding maximum acceptable CRUD levels, but its influence has to be considered both on the thermal models as well as on the corrosion kinetics models. An acceptable amount of CRUD might be also driven by operational limits on AOA (axial offset anomalies), especially if the CRUD deposition is not homogeneously distributed within the core. With the severe thermal duty that occurs with fuel management strategies supporting extended cycle length and core power uprates, and in order to reduce radiation levels in plant components, strategies:

a) with modified water chemistry with e.g. higher lithium concentration, resulting in higher pH values;
b) with the injection of Zn or Fe into the primary coolant for reducing dose rates or increased corrosion protection;

have been introduced in the last years.

This chemistry, for example, has proven to be adequate to control CRUD deposition. Notwithstanding that, as the plants in transition to longer operating cycles require extra loading of soluble boron at beginning-of-life, to maintain the pH at the required level (around 7.2) with this boron concentration the fuel has to be operated with high lithium concentration – above 2.2 parts-per-million (ppm) for some time, which could increase the corrosion rate.

There is also a concern with large porous CRUD depositions in PWRs leading to boron pick-up, thereby causing distortion of the core axial power profile and reduced SDM. Usually, the transition to modified water chemistry is guided by monitoring the corrosion behavior of the fuel rod cladding. In the past, criteria on CRUD deposition were considered “derived” criteria, and only indirectly safety related.

However, unexpected large amounts of CRUD were observed at the River Bend facility (United States) during the Cycle 8 and Cycle 11 outages. CRUD-induced localized cladding corrosion failure of fuel was observed. NRC is currently is considering to introduce limits for CRUD.

Stress/strain/fatigue

Generally conservative design limits are taken for cladding stress (e.g. around 0.2% yield or tensile strength at operating temperature). For strain, there are several different but closely-related limits. To add to the confusion, there are actually several forms of the so-called “1% strain” criterion in use.

The first “1% strain” criterion applies on the long-term strain that occurs after gap closure induced by outer overpressure (creep down). The process includes thermal expansion, but it is dominated by the swelling process. There are two different values used, the 1% strain criterion relates to the tangential (circumferential) strain only, and the 2.5% strain criterion relates to the combined tangential and axial strain, the so-called equivalent strain, which is the vector addition from tangential and axial direction. The 1% tangential strain and 2.5% equivalent strain are approximately equivalent in terms of cladding load.

This strain limit applies for Condition I and II events. This pellet-cladding mechanical interaction (or PCMI) phenomenon is caused by a combination of cladding creep, fuel swelling (which reaches a maximum at end of fuel life), fuel rod internal pressure (Section 3.8), and fuel pellet thermal expansion. The margins from these limits to actual failure stresses and strains are defined from the fuel vendor's database for a particular fuel, cladding, and burn-up range.

In some countries, the 2.5% criterion has been replaced by the value of 3.5%. The reason for the increase from 2.5% to 3.5% was the need for higher burn-up (>60 MWd/kg). It has been shown that, for some cladding materials, that 3.5% strain will have enough margins to failure. The second 1% strain criterion is used to define the maximum load that can be sustained by a fuel rod for a short time.

This strain criterion (tangential, transient strain) was postulated independent of the DNB criteria, which should limit the power before the 1% strain limit is effective. Up to now, this second criterion has been used as a pellet-cladding mechanical interaction (PCMI) criterion for operational over-power transients (Condition II events).

However, it is clear that this criterion alone will not prevent the cladding from PCMI or PCI failures. The DNB-criterion may be more restrictive. For PCI prevention operational rules are established, e.g. limiting the absolute value of power increase and the power increase velocity. These rules apply above a certain power level and depend on the so-called cladding condition, which is the time at a certain power level.

The cumulative number of strain fatigue cycles on each fuel assembly structural member is assumed to be significantly less than the design fatigue lifetime, which in turn is based on appropriate data and usually includes a safety factor on stress amplitude and a safety factor on the number of cycles.

<u>Oxidation and hydriding</u>

Oxidation and hydriding under normal reactor operating conditions directly impact fuel performance, not only during normal operation, but during transients and accidents as well. Cladding corrosion or oxidation degrades material properties, most importantly the effective cladding-to-coolant heat transfer (with a subsequent increase in fuel temperature and thus the stored energy of the fuel).

Hydrogen absorption by the cladding and subsequent formation of hydrides may lead to cladding embrittlement; these phenomena are increasingly important at higher exposures for some of the existing alloys; the significance of the issue is much reduced for modern alloys with improved corrosion and hydrogen pickup performance. For these reasons, the composition and fabrication of zirconium based cladding materials have become highly optimized.

For BWRs, where Zircaloy-2 is used, the thermal-mechanical processing is optimized to result in an intermediate second phase particle (SPP) size. Too large a SPP size could result in nodular corrosion, but too small a SPP size could result in higher uniform corrosion. Although Zircaloy-2 continues to be used in BWRs, Zircaloy-4 has been largely replaced in PWRs by low-tin outer liner cladding concepts (DUPLEX) or by Zr-Nb and Zr-Nb-Sn alloys (e.g. ZIRLO, M5, MDA and E110).

Uniform oxidation or corrosion rates differ between PWRs and BWRs.

a) The heat flux overlaps between PWRs and BWRs.
b) The lower operating temperature of BWRs means lower oxide and thus lower hydrogen pickup and thus the cladding typically does not form a hydride rim.
c) The formation of a hydride rim in PWRs causes corrosion acceleration which lead to further thermal feedback.

With the lower operating coolant temperature, uniform corrosion is much less critical for BWRs; in contrast, PWRs are less susceptible to nodular or local phenomena (e.g. CRUD-induced localized corrosion, enhanced shadow corrosion) due to much less oppressive heat transfer and flow conditions as well as a reducing chemical potential at low oxygen level. The reduced oxygen potential is more important and could be the primarily reason for the difference.

Regarding uniform corrosion and hydrogen pick-up, the following tendency has been observed. For BWR Zircaloy-2 cladding, corrosion is low, however, hydrogen pick-up rate increases significantly as oxide thickness increases. Hydrogen pick-up may also depend on in-core residence time. For PWR cladding, hydrogen pick-up rate remains almost constant up to high burn-up. However, due to the thermal feedback effect on corrosion temperature uniform corrosion increases at higher oxide thicknesses/high burn-ups 9 which is of special relevance for non-optimized cladding materials (e.g. standard Zircaloy-4). This effect depends on power and the power history. The understanding of the accelerated corrosion at high burn-up may be unclear. As the oxide thickens (at higher burn-up), the temperature may not necessarily increase due to the lower power level at elevated burn-up. The accelerated corrosion may be due to the formation of hydride rim, which only applies to alloys with hydrogen pickup of more than ~350 ppm.

For design purposes, oxide thickness and hydride concentration limits are normally assumed at end of fuel life. For traditional alloys (e.g. Zircaloy) values are usually in the range of 100 micron and 500-600 ppm (wall averaged value), respectively; these values are taken from post-irradiation examination (PIE) data, and represent reasonable bounds on data measured from fuel exposed in commercial PWRs. The same limits are sometimes also used for modern alloys that exhibit a much lower corrosion propensity. The relevance of these criteria is questionable in this case because such high levels of oxidation and hydriding have never been observed under reactor operation conditions.

Post-irradiation examination data from high burn-up fuel show that local oxidation thickness and hydrogen content may exceed current design limits. From a cladding integrity point of view, local oxide thickness and hydrogen contents could be more limiting and important than average ones. Especially, in the case of hydrogen content, circumferential non-uniform distribution or hydride rim might be important factors for cladding's mechanical integrity during normal operation and postulated design basis accidents.

An oxidation effect which has not been much considered in the past and which could be important is fuel bonding induced internal oxidation of the cladding (it may be called "chemical bonding" instead of "oxidation"). At increasing burn-up the pellet-cladding gap in the fuel rods tends to close due to swelling and cladding creep-down and a bonding layer is formed between the pellet and the cladding. This bonding layer may have a deteriorating effect on fuel rod behavior under irradiation, but may have a beneficial effect for pellet-cladding interaction/stress corrosion cracking (PCI/SCC). This has been proven from high burn-up ramp tests at high burn-up and post-tests examinations: beyond a certain burn-up threshold (around 45 GWd/t) fuel rods do not fail any longer by SCC-PCMI; it is assumed that the bonding layer acts as a barrier to the corrosive fission products (iodine) generated in the central part of the pellets.

Extremely high hydrogen content may affect current 1% strain limit:

a) Cladding materials have been tested with significantly high hydrogen content and survived at the 1% strain limit.
b) Only in the cases of stress corrosion cracking does the cladding fail below 1% strain.

Another concern is hydride re-orientation. It is known that hydride re-orientation will occur under certain temperature histories and tensile stress conditions, especially for re-crystallized cladding. Radial hydrides may result in more degradation of cladding mechanical properties. Additional issues, such as the oxide cladding spalling and very high local concentration of hydrides in the cladding wall, are not covered by the present limitations but are addressed when RIA limits are determined. Also, from a LOCA performance point of view, a high hydrogen content may lead to degradation of residual cladding ductility.

In summary, as corrosion of some of the traditional zirconium-based alloys is probably one of the leading parameters that limit the lifetime of nuclear fuel, there is a rationale for reviewing the adequacy of the current applicable limits on maximum local oxidation and hydriding levels in the cladding, especially in view of the performance of highly burnt fuel.

Rod internal gas pressure

Increases in fission gas release can lead to high fuel rod internal pressures and could also lead to a deterioration of the thermal conductivity of the gas in the plenum/fuel rod free volumes and, more importantly, of the heat transfer between the pellets and the cladding due to the resulting gap size modification. The fission gases Xe and Kr decrease the thermal conductivity of the helium gas in the gap, which increases the fuel temperature; when the gap is closed, this effect becomes less significant. This induces a feedback mechanism since an increased fuel temperature enhances the fission gas release.

Due to the above mentioned thermal feedback mechanism and the sensitivity to changes in rod power level, the fission gas release in various rods can be highly irregular. The high internal rod pressures can have an important effect on fuel cladding under transients and postulated accidents behavior (ballooning, burst, etc.). For example, during a LOCA, the pressure differential across the cladding wall may be inverted within seconds due to early complete system pressure drop. Two alternative criteria for acceptable internal gas pressure are currently used in various countries by their regulatory authorities.

In the first option, the rod internal pressure is held below the nominal pressure in the reactor coolant system (RCS) during normal operation in order to prevent outward creep of the cladding. In the second option, the rod internal pressure may exceed the RCS pressure, but is limited so that the cladding creep-out rate due to an internal rod pressure greater than the reactor coolant system pressure is not expected to exceed the fuel swelling rate, i.e. the fuel to cladding gap does not open (this is the so-called “no lift-off” criterion). The two alternative criteria – absolute (reactor coolant system pressure) or relative (cladding lift-off) – for acceptable internal gas pressure are currently used in various countries by their regulatory authorities.

At high fuel burn-up either criterion could, in transient and accident conditions, lead to a high internal pressure of the fuel rod with a possible effect on stored energy, cladding ballooning and bursting, which could challenge core cool-ability, and thus the level/limits resulting from this safety criterion. The consequence of rod internal pressure build up at elevated burn-up has been extensively studied for the last ten years. One such study, a lift-off test series (IFA 610) with UO_2 and MOX fuel rods of 50-60 GWd/t was performed at the Halden research reactor.

For the UO_2 rod, lift-off occurred at an overpressure (above the system pressure) of around 130 bar; results for the MOX fuel rod indicate an even higher lift-off pressure.

On the basis of the Halden findings, as well on the basis of the Studsvik ROPE (Rod Over Pressure Experiments) data, the "no lift-off" criterion was proposed by some vendors, thus considering outward creep rate/strain and tensile stress due to overpressure. Nevertheless, the high overpressure thresholds found in the Halden experimental program show there are significant margins regarding cladding lift-off, but they cannot be used directly as safety criteria; rod internal pressure has to be limited to comply with other requirements, in particular for the spent fuel (to avoid hydride reorientation during transportation in dry casks for instance).

For MOX fuel, the fission gas and helium releases are higher as compared to UO_2 fuel, and the adequacy of the relative (liftoff) criterion for acceptable internal fuel rod gas pressure may also require further study. This is not a matter of the MOX fuel itself but more a matter of the related reactivity history expressed in the power histories (see next sentence). An acceleration of fission gas release with exposure is observed, also because of the higher linear heat generation rate in high burn-up MOX due to the higher reactivity level.

The reactivity level itself may not be higher, but the power histories are more demanding. The development of rod internal pressure as function of burn-up on MOX fuel needs to be well characterized, also in consideration of the production method and plutonium content in the MOX fuel. These criteria should not be fundamentally affected by design changes, although methods to demonstrate compliance will be affected.

Thermal mechanical loads and PCMI

Pellet-cladding mechanical interaction (PCMI) refers to the stress and/or strain on the cladding from an expanding pellet, especially during a transient. Pellet expansion results mainly from thermal expansion and gaseous swelling, and if the stress is large enough it can result in cladding failure. PCMI differs from the related SCC-PCI phenomenon in as much as the latter refers to power ramps (with sufficiently high power levels, sufficiently high ramp rates) where the stress is held for a relatively long period of time and corrosion is necessary for cracking to take place.

The avoidance of mechanical fracture of the cladding during transients due to pellet-cladding mechanical interaction, which is the basic safety criterion, is partially covered by the limit on uniform cladding (plastic and elastic) transient strain of 1%. However, PCMI-induced failures can occur at local strain levels well below 1% – particularly for brittle cladding at high burn-up (if the cladding exhibits a highly concentrated outer hydride rim).

The cladding mechanical property degradation is one of the most important issues for high burn-up fuel utilization, and the in-reactor performance of the cladding strongly depends on the cladding material properties. The cladding material properties for high burn-up fuel should be carefully examined in order to reflect these data properly. A range of power-increasing transients where PCMI may be important are addressed in plant and reload licensing safety analyses (e.g. loss of feed-water heating in a BWR and steam-line break in a PWR – a safety level that may not require fuel rod integrity).

If the PCMI stress is low enough or if the cladding ductility is high enough, PCMI will not be the mechanism for cladding failure. In those cases, the cladding temperature would rise because of the increasing power, and eventually critical heat flux might be exceeded and lead to cladding damage. For the latter transients CPR/DNBR fuel integrity criteria are generally limiting, and these transients are usually analyzed from this perspective (i.e. without looking at PCMI).

Several things might occur at high burn-up that could result in early cladding failure by PCMI:

a) First, the pellet-to-cladding gap reduction will eliminate some free expansion of the pellet prior to contact with the cladding.
b) Second, the large accumulation of fission gas on fuel grain boundaries will also expand during a hold period after a power increase, which would contribute to the cladding strain, which is called the gaseous swelling phenomenon (the most important part of gaseous swelling is a delayed gaseous swelling contribution, the instantaneous gaseous swelling contribution is much lower).
c) Third, cladding ductility is progressively reduced by radiation embrittlement already at intermediate exposure such that a mechanical failure might be considered (but actually such a failure mechanism is never activated because the pellet expansion is a strain driven loading and in such condition the cladding can withstand several percent strain without failing).
d) Fourth, if cladding hydriding further reduces the ductility of the cladding waterside at high exposure, mainly at lower cladding temperatures, PCMI failures could occur for those transients that were CPR/DNBR-limited before, and thus the critical heat flux type of analysis would then be inappropriate for safety evaluation.

At intermediate burn-ups, the only failure mechanism is SCC-PCI, which has very little to do with the cladding mechanical properties. Except the fact that the pellet-cladding gap closure kinetics may play a role: if the low stress creep rate of the cladding is low, the gap closure intervenes at a higher burn-up, when the pellet rim and the bonding layer exhibit beneficial effects, impairing stress corrosion incipient cracks on the inner side of the cladding.

Another failure mechanism is so-called outside-in cladding failure due to delayed hydride cracking, which has been observed on ramp tested high burn-up BWR fuel with Zr-liner in Japan:

a) According to the test results obtained so far, the fuel failure is dependent on local power level and holds time at the terminal power level;
b) This type of fuel failure mechanism must also be taken into account.

Experimental data on PCMI for light water reactor fuel cover a range of burn-up up to 60 GWd/t; so far, none of these results point towards PCMI effects being prohibitive at high burn-up.

The thermal-mechanical limit (a burn-up dependent curve) is established while including the PCMI phenomenon, as well as various other phenomena (fuel rod internal pressure, stress/strain, fatigue, fuel melting, cladding corrosion and ballooning). In some countries, the limit is set to bound all these effects; also, the limit includes the effect of thermal and mechanical overpower during normal transients (AOO).

The basic safety criterion – the maintenance of fuel rod integrity – is not affected by new design changes; however the supporting strain limit (1% strain) may change.

For high burn-up cladding, the tendency to pick up hydrogen and the hydride morphology may be the most important issues in determining strain limit, and the presence and orientation of hydrides will depend on parameters such as:

a) cladding final heat treatment during fabrication (stress relieved versus re-crystallised);

b) burnup level;

c) thermal-mechanical loads (inducing stresses).

Pellet cladding interaction (PCI)/stress corrosion cracking (SCC)

Some pellet cladding interaction (PCI) failures may be associated with stress corrosion cracking (SCC) in the cladding material, and are dependent upon local power ramps during reactor startup or maneuvering (e.g. rod adjustments/swaps, load follow) and during normal transients (AOO). Both the stress from the power increase and the corrosion level (chemical component, e.g. iodine) at the pellet-cladding gap are necessary conditions for SCC-PCI.

A crack, initiated at a microscopic defect in the inner side of the cladding, propagates until the stress in the remaining load-bearing part of the cladding exceeds the ultimate tensile strength, resulting in failure. Fresh fuel rods usually do not fail by SCC-PCI (because the gap is too widely open and consumes most of the power change until it closes and also because there are not enough corrosive component available). Neither do fuel rods operated at constant power fail from this phenomenon.

The SCC-PCI phenomenon has been extensively investigated after SCC-PCI failures were noted in operating reactors. To control the PCI phenomenon, operating rules (also called pre-conditioning interim operating management recommendations, or PCIOMRs) to limit local power increases and "condition" fuel to power ramping were implemented.

These rules are usually a function of exposure (at intermediate burn-ups, when the gap is closed and the contact pressure is high, the fuel is less able to withstand ramping). In fact, the feedback from the ramp test database shows that SCC-PCI behavior may differ between various fuel types depending on the pellet-cladding gap closure kinetics. It has been also observed that premature SCC-PCI failures can occur due to high local stress induced by pellets with "chips" or missing pellet surfaces (MPS) generated during the pellet manufacturing process.

The PCI limits/rules typically contain a maximum ramp rate for reactor power increase (W/cm/hr), a maximum "single step" power increase (W/cm), and a threshold (in W/cm) above which such power increase limitations apply and a minimum time-period after which the fuel may be considered (pre)conditioned to larger power ramps. In response to this issue, PCI resistant fuel types were developed. Notably, a small layer of zirconium ("barrier" or "liner", with or without small additives like Sn or Fe) was added at the inner part of the cladding as a remedy in BWR fuel designs.

In addition, the modern fuel assemblies contain more fuel rods and therefore have a lower linear heat rating for each rod: this way, the fuel may permanently operate below the PCI threshold and thus not be in danger of failure from this mechanism. Another way of reducing the SCC/PCI risk of failure consists in filling the cladding with very short pellets, which reduce the "hour glassing effect" during power transients and thus the stresses levels applied to the cladding.

In a number of countries, PCMI limits and SCC-PCI limits related to manufacturing-induced defects, such as MPS, are not licensed. However, operating limits (particularly the I-131 concentration level in the primary coolant) will bind plant operation. In other countries, specific criteria are established to prevent

PCI failures. This is particularly important where complex operation conditions, such as load follow or extended periods at low power level, are in effect.

Pellet-cladding interaction rules do pertain to safe fuel performance, and regulators will maintain that for non-PCI resistant fuel these limits be adequate and that reactors obey these rules for core operation. The SCC-PCI limits should be kept updated, to be in concord with the respective fuel and core design envisaged; this is primarily done by performing ramp tests. At present there is a good basis for SCC-PCI limits up to and beyond 50 GWd/t.

Fuel melting

Traditional practice in the design of light water reactor fuel has assumed that failure will occur if centerline melting takes place. This analysis is performed for the maximum linear heat generation rate throughout the core, including all hot spots and hot channel factors, and it normally accounts for the effects of burn-up and fuel composition (e.g. Pu or Gd content) on the melting point.

According to Section 4.2 of the NRC Standard Review Plan:

a) Overheating of fuel pellets - it has also been traditional practice to assume that failure will occur if centerline melting takes place:

    I. This analysis should be performed for the maximum linear heat generation rate anywhere in the core, including all hot spots and hot channel factors, and should account for the effects of burn-up and composition on the melting point;

    II. For normal operation and anticipated operational occurrences, centerline melting is not permitted. For postulated accidents, the total number of rods that experience centerline melting should be assumed to fail for radiological dose calculation purposes;

    III. The centerline melting criterion was established to assure that axial or radial relocation of molten fuel would neither allow molten fuel to come into contact with the cladding nor produce local hot spots. The assumption that centerline melting results in fuel failure is conservative.

After even low exposure, the radial power distribution in a fuel pellet peaks at the pellet surface rather than the pellet centerline. As a result, the maximum temperatures during a “rapid” transient may occur at locations other than the centerline. This is the opposite of normal operation or the “slow” power transient, where the maximum temperature is expected at the fuel centerline.

To accommodate the rapid transient effects, the applicability of this paragraph in the NRC Standard Review Plan could be improved by substituting the word “fuel” for the word “centerline” in a number of places. For both normal operation and anticipated transients, centerline melting is not permitted. A reason for the criterion is that the transition from the solid to the liquid phase of UO_2 is accompanied by an increase (~13%) in volume. During melting, an expansion equal to a linear strain of 0.043 occurs”. This is equivalent to a volumetric expansion of 3 x 0.043 = 0.129.

The centerline melting criterion was established to assure that axial or radial relocation of molten fuel would neither allow molten fuel to contact the cladding nor produce local hot spots. Regulatory guidance generally contains an explicit limit with regard to fuel melting during both normal operation and anticipated operational occurrences.

Further, fuel melting may be used as a cladding failure criterion for some design basis accidents, and for these postulated accidents, the total number of rods that experience centerline melting is assumed to fail for radiological dose calculation. For more aggressive conditions, present during some transients and accidents, this image is not representative.

For example, during one hypothetical accident, the rod ejection event, NRC Regulatory Guide 1.77 states:

a) For UO2 fuel, a large fraction of this generated nuclear energy is stored momentarily in the fuel and then released to the rest of the system;

b) If the fuel energy densities were high enough, there would exist the potential for prompt rupture of fuel pins and the consequent rapid heat transfer to the water from finely dispersed molten UO_2;

c) Prompt fuel element rupture is defined herein as a rapid increase in internal fuel rod pressure due to extensive fuel melting, followed by rapid fragmentation and dispersal of fuel cladding into the coolant.

It should be understood that fuel melting does not necessarily result in failure of the fuel or the fuel cladding. Assuming steady-state conditions, the centerline temperature in commercial light water reactor fuel reaches the melting point at power levels in the range of 82 kW/m (25 kW/ft). Linear power levels in operating reactors are generally much lower because they are limited by other, more restrictive conditions (e.g. thermal-hydraulic limits), but fuel melting continues to be avoided by design. The melting point of UO_2 is usually assumed to decrease with increasing burnup. However, it must be noted that melting points were measured in Japan on high burn-up fuel (around 60 GWd/t) and the results suggest no clear reduction of melting points. Due to the difficulties in measuring melting point in high burn-up fuel samples, the results are not conclusive.

<u>LHGR limits</u>

Linear heat generation rate (LHGR) limits are the most limiting of (1) thermal-hydraulic, (2) loss-of-coolant, and (3) thermal mechanical limits. Figure 8 (taken from a plant final safety analysis report - FSAR) schematically shows how these limits are combined. This figure shows the limiting local linear heat generation rate for several different criteria as a function of core height. A typical envelope of operating power shapes is (must be) bounded by all of the criteria-based lines.

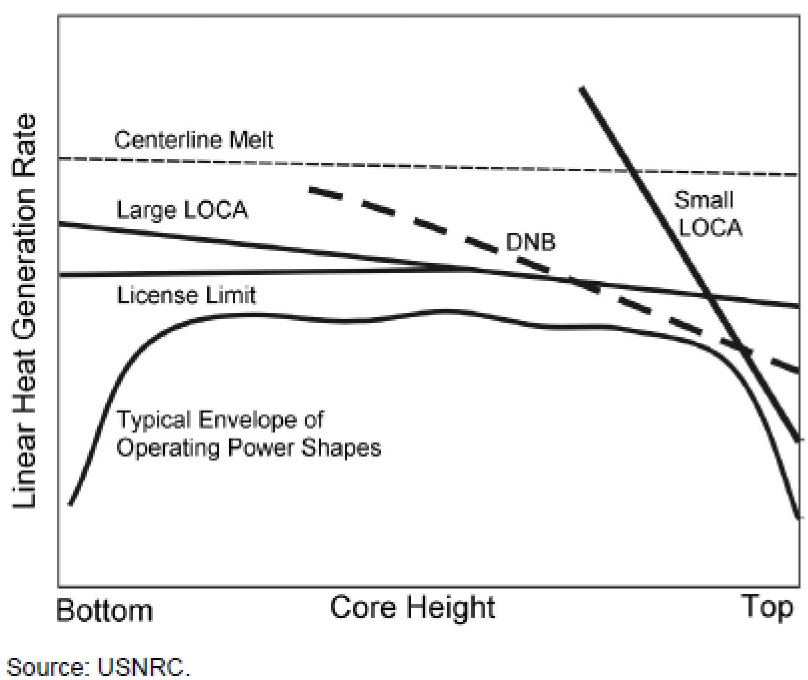


Figure 8. Combination of limits in FSAR

Figure 9 is a similar figure, but the maximum linear heat generation rate for various fuel safety criteria is plotted as a function of burn-up rather than core height. The figure schematically shows how these fuel (safety) limits are combined. In this figure, the cladding 1% strain limit is lowest at beginning of life. Fuel melting and other fuel safety criteria show a modest decrease with burnup. In deriving the LHGR limits, the most limiting of fundamental criterion (e.g. DNB) is plant, burn-up, and fuel design dependent.

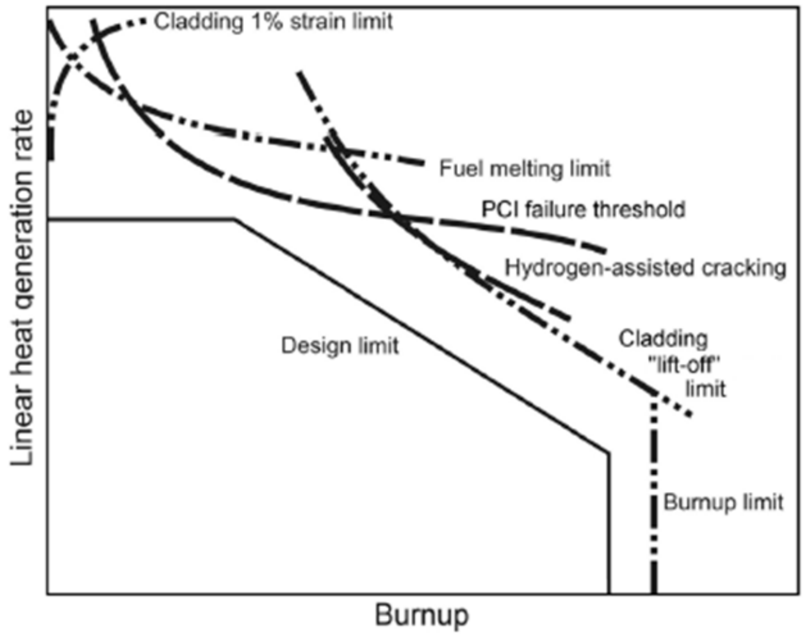


Figure 9. Another representation of combination of limits

<u>RIA cladding failure</u>

For a RIA, the number of fuel rod failures must be calculated so that the radiological doses to the public can be estimated. In many countries, the cladding failure limit for RIA is based on the NRC standard review plan, which suggests a maximum radially averaged fuel enthalpy of 170 cal/g for BWRs and the DNB criterion for PWRs.

Based on RIA experiments at CABRI and NSRR with fuel rods at a burn-up of approximately 50 GWd/t or higher, an assessment of the adequacy of this limit appeared desirable. In this respect, various limit values as function of burn-up have been proposed based either on direct experimental data (full-scale tests) renditions or on relevant parameters, such as cladding oxide thickness and mechanical properties. Results for RIA tests (SPERT, PBF, CABRI, NSRR) with Zircaloy-cladding fuel above 5 GWd/t show failures from a pellet-cladding mechanical interaction (PCMI) rather than high-temperature failures related to critical heat flux.

It is believed that the reduction in gap size, reduced ductility due to irradiation and embrittlement due to hydrogen absorption are all responsible for the change in cladding failure mechanism. For irradiated fuel the fission gas induced fuel swelling is another contributing factor. Thus the effects of burn-up appear to enhance the loading to the cladding and/or alter the failure mechanism and make the critical heat flux (as a stand-alone criterion) inappropriate.

In Japan, the threshold of PCMI failure in terms of enthalpy increase (cal/g UO2) has been determined in terms of burn-up (GWd/t) – see Table 7.

Table 7. RIA criteria as function of burn-up

Burn-up (GWd/t)	Enthalpy increase (cal/g UO_2)
<25	110
25-40	85
40-65	50
65-75	40

Fuel fragmentation and fuel dispersal

Fuel fragmentation refers to situations for which the fuel cladding breaks into pieces and fuel dispersal to situations for which fuel particles escape from the cladding following a rupture. Although fuel fragmentation is traditionally considered to exist only in conjunction with highly energetic events such as the reactivity-initiated accidents (RIA), recent results from the Halden test reactor show that fuel fragmentation can also occur during the loss-of-coolant accident (LOCA).

To avoid the loss of coolable geometry and the generation of coolant pressure pulses, peak fuel enthalpy criteria are used as limits for RIA. Historically, a radially averaged fuel enthalpy value of 280 cal/g has been used in the United States and other countries based on data from early RIA fragmentation measurements prior to 1974 on fuel with a maximum burn-up of 33 GWd/t (e.g. SPERT and TREAT tests in the United States): this value corresponds to the melting of UO_2 which causes fragmentation of the cladding and expulsion of fuel particles. Later refinements in the measurements and in the definition of the fragmentation enthalpy value, as well as PBF-RIA tests led to reductions of the 280 cal/g limit. Accordingly, various regulatory authorities use a lower value for the enthalpy limit.

Experiments in the French CABRI test reactor and the Japanese NSRR test reactor using high burn-up fuel samples have resulted in fuel particle dispersal for deposited energies well below 200 cal/g. It is clear that, for high burn-up fuel, a mechanism other than fuel melting is producing particle dispersal at low deposited energies. This new mechanism may possibly be related to the large accumulation of fission gas bubbles on grain boundaries of the fuel and the rapid expansion of that gas during the power pulses, with special emphasis on MOX clusters. Entrainment of particles in escaping fission gas may also be involved.

Various effects (pulse width, cladding type, coolant type, internal pressure, coolant temperature), some of which are not yet well understood, may play a role: however the burn-up effect in UO2 is evident. For RIA, the current practice is often to define criteria intended to ensure that there is no fuel cladding failure, thus preventing from fuel dispersal and fuel fragmentation.

Of special interest is the formation of a peripheral zone in the fuel material with high plutonium content and consequently high reactivity, porous structure and high content of fission products, the so-called RIM, which grows as a function of exposure: at about 45 GWd/t the RIM-zone is of the order of 60 μm.

Studies are continuing to clarify what role the RIM-zone plays in transient accident situations and when grain fragmentation occurs during the RIA transient: during the pulse or during the cooling phase when the pellet becomes less constrained by the cladding. This point is crucial: if the grain fragmentation occurs during the pulse, all the fission gases that were trapped in the RIM are suddenly released and may add a pressure considered in PCMI loading. For reactivity events, it may be correct that the fuel dispersal limit is in the range of around 230 cal/g.

This may be sufficient to ensure a coolable geometry for fresh and very low burn-up fuel during this energetic event. However, for both LOCA and fuel at high burn-ups, there is a need for further understanding of the fuel dispersal process and the effects of high burn-up (in particular the effect of the RIM-zone and the MOX clusters).

Non-LOCA cladding embrittlement/temperature

Certain non-LOCA accidents are analyzed to estimate radiological doses to the public and to demonstrate that coolability of the core is maintained. For accidents like the PWR locked rotor accident, DNB is used to indicate cladding failure for dose calculations, and the peak cladding temperature of 1480ºC (2700ºF) is sometimes used to demonstrate coolability.

The 1480ºC limit was taken from early data estimates of the fuel failure boundary for LOCA conditions (1480ºC and 17% of cladding thickness oxidized by metal-water reaction). This limit was established in the 1969-1971 time period prior to the ECCS hearings in the United States, which resulted in a lower temperature limit for LOCA analysis (2200ºF or 1204ºC). The rationale for retaining a higher temperature limit for non-LOCA transients was that those transients were of brief duration and fuel rods could withstand brief periods of DNB without suffering serious damage.

The 1482°C limit corresponds to one fuel vendor's opinion of the maximum cladding temperature beyond which the oxidation is thermally self-sustained (due to endothermic reaction). Other (different) limits were also proposed.

LOCA cladding embrittlement

For LOCA analysis, it is generally assumed that a certain amount of fuel rods fail and release fission products, but that emergency core cooling systems (ECCS) operate in such a way that fuel rod fragmentation is avoided, thus preserving a coolable geometry, and moreover provide long-term core cooling.

Based on many laboratory quenching and ductility tests (or strength-based tests) with unirradiated Zircaloy tubes, it was found that cladding would not become embrittled enough to fragment if the peak cladding temperature remained below 1204ºC (2200ºF) and the total oxidation did not exceed 17% of the cladding thickness prior to transient based on the Baker-Just oxidation correlation. These cladding embrittlement criteria (ref. 10CFR50.46) are used widely, although in some cases the oxidation limit is placed at 15% (e.g. Japan). In addition there is a LOCA limit on core-wide hydrogen generation, however this is for containment integrity and not against embrittlement (limit is usually 1% related to total possible cladding oxidation).

The cladding embrittlement criteria were developed in the 1960s and early 1970s; experimental verification and validation included tests with zero or low burn-up fuel. Nowadays fuel operation exhibits typical oxidation levels of up to 100 microns or more and hydrogen concentrations up to or even higher than 600 ppm at the time of discharge (these levels are usually employed as criteria for fuel mechanical design, which frequently become licensed limits).

Hence the 17% criterion is now often interpreted as "total" oxidation level. As the oxidation process at LOCA temperatures differs from that at normal operating temperatures, this interpretation may be considered as being very conservative; the question whether the oxidation during normal operation should be accounted for when comparing against the 17% LOCA-limit is unsettled. In Europe, the total oxidation must be considered. In the United States, consideration of pre-transient oxidation is suggested but not required. A different criterion, that might be more suitable especially at high burn-up, could also be envisaged.

There is a number of issues and concerns that necessitate additional verification and subsequent justification or adjustment of the current LOCA limits. Some of these are related to high burn-up:

a) radiological consequences: extent of rods burst, and fission product release; consequence from hydrogen-induced β-phase stabilizing effect on cladding strain;

b) consequences from fine fragmentation of the fuel, filling the space in the ballooned cladding (also the timing of slumping of the fuel column into the ballooned area is important, as well as the consequences of additional decay heat related to cladding temperature and oxidation);

c) cladding behavior during quench after high temperature oxidation, and long-term cooling: changes in oxidation rate and cladding embrittlement, heating and cooling rates;

d) UO_2-RIM or MOX clusters fission gas induced fuel swelling (losses in mechanical strength may become important if cladding wall strength is significantly weakened during irradiation).

Whereas some are of a more generic nature:

a) fuel relocation in the ballooned region and its impact on the calculated peak cladding temperature (PCT);

b) potential subchannel blockage (interaction between ballooned areas, axial extent of the ballooned areas).

<u>Blowdown/seismic/transportation loads</u>

During a seismic event the fuel assemblies are subjected to dynamic, structural loads which could cause fuel assemblies to sway back and forth, causing impacts with each other and with the vessel wall. Jet forces associated with blowdown from one side of the vessel through a broken pipe could also accelerate the vessel in the lateral direction, resulting in similar impacts between fuel assemblies and vessel wall. Most countries follow the safety criteria as per NUREG-0800, SRP 4.2, Appendix A which require core coolability and control rod insertability to be assured under the combined seismic and LOCA loads.

These criteria are often translated into design requirements such as:

a) fuel rod fragmentation shall not occur (can be met by verification that fuel rod stresses are within limits);

b) control rod insertion shall not be impaired (verify that combined loads do not displace the fuel assembly from the support piece);

c) limit spacer distortion to ensure rod coolability (verify that spacer distortion or failures either do not occur or do not decrease the hydraulic section of the grid cells).

Design requirement changes on allowable structural loads for earthquakes during and after a LOCA may be needed at high burn-up, because the strength and ductility of high burn-up cladding, guide tubes, grid spacers (PWRs), and channel boxes (BWRs) will not be the same as for fresh material. Analyses for fresh fuel usually show ample margins, and the increased strength at high burn-up would seem to enlarge those margins. But the method of review presumes that the material being analyzed is ductile, whereas a substantial loss in ductility occurs at high burn-up for some materials.

It should be noted that the alteration at high burn-up of the buckling strength of the spacer grids is not related to the properties changes of the grid materials but to the grid-to-rod spring relaxation under irradiation. Altered materials properties (growth, creep, ultimate stress and strain, etc.) for high burn-up cores and for new core materials may well affect the results of this structural analysis; thus, adequate treatment of these properties is needed, which implies that material properties verification at high burn-up is of importance. Safety criteria in this area are not directly affected by the new design changes.

<u>Assembly hold-down force</u>

LWR fuel assemblies are equipped with hold-down springs in the top piece. They have to provide sufficient forces to prevent fuel assembly lift-off due to hydraulic loads during normal operation and anticipated operational occurrences, with the exception of the hot pump over-speed transient (for the hot pump over-speed transient some lifting is tolerated; the hold-down springs shall again prevent fuel assembly lift-off after the transient has subsided). Safety criteria are usually defined following NUREG-0800, SRP 4.2, Appendix A: vertical lift-off forces must not unseat the lower fuel assembly tie-plate from the fuel support structure. The required hold-down force is calculated by Equation (4.8):

(4.8)

$$F_{HD} = F_{HY} + B - W$$

where

F_{HD} required holddown force

F_{HY} hydraulic force

B buoyancy force

W fuel assembly weight.

The fuel assembly hold-down force leads to compressive forces on the guide tubes, which forces can give high fuel assembly bow due to irradiation induced guide tube creep. Vice versa, high compressive forces can result from excessive guide tube growth. Guide tube growth is correlated to the fast neutron fluence and hydrogen pickup. Therefore a major consideration at high burn-up levels, where high corrosion and hydrogen pickup of the guide tube accelerate guide tube growth above the fast neutron irradiation induced rate. Corrosion and hydrogen pickup highly depend on the coolant temperature and on the guide tube material and its condition. Thus, to ensure acceptable guide tube corrosion and hydrogen pickup, guide tube design and material has to be selected adequately.

Fretting wear

Fretting wear at contact points on the fuel assembly structural members should be limited. Fretting wear tests and analyses that demonstrate compliance with this design basis should account for grid spacer spring relaxation. The allowable fretting wear should be stated in the safety analysis report, and the stress strain, and fatigue limits should presume the existence of this wear. It is noted that wear cannot be analytically predicted at this time. As a consequence, compliance with the fretting wear limit (typically 10% of the cladding thickness) is checked a posteriori, through post-irradiation examination:

a) Spacer grid structural tests, control rod structural and performance tests, fuel assembly structural tests (lateral, axial and torsional stiffness, frequency, and damping), fuel assembly hydraulic flow tests and endurance tests (lift forces, control rod wear, vibration, fuel rod fretting) are the necessary tests to determine if a specific fuel assembly design is sensitive (or not) to the fretting wear phenomena.

b) To simulate the effect of irradiation those tests should account for spacer spring relaxation.

Coolant activity

In most countries, limits are specified in the plant technical specifications on the concentration of I-131 (sometimes also of Cs-137) in the primary coolant; numbers are typically around $2 * 10^9$ Bq/t. Thus NPP operation with a certain (small) number of fuel failures is tolerated; the plant systems have been designed to cope with fuel failures of this magnitude. Aside from this technical specification limitation, no fuel safety criteria on coolant activity exist. Usually, as soon as larger I-131 concentrations are measured that would challenge the technical specification limit, the plant operational staff prepares for plant shutdown to identify and replace the leaking bundles, so that plant operation within technical specification limits may be continued. From large cracks in the fuel rods (direct contact between fuel and coolant) washout of fuel material from the pellet may occur, subsequently leading to a high concentration of Neptunium. Even after the leaking fuel has been removed, it may take a long time (several years/cycles) for this concentration to decrease as fuel material is plated out throughout the primary system.

Fuel gap activity

Fuel gap activity is of interest for accident scenarios that may result in cladding failures but that do not involve melting of the fuel. It determines the potential release of fission products to the primary circuit. During normal reactor operation, some fission products come out of the UO_2 fuel matrix and collect in the gap between the fuel pellet and the cladding. Fixed values of release to the gap, like up to 10% of the rod inventory for noble gases and 1-6% for halogens and alkali metals, are assumed in safety analyses. These gap activities are then assumed to be released from failed fuel rods for the purpose of off-site dose calculations for postulated accidents. The release fractions assumed are not safety criteria, but represent conservative numbers used for assessment of the design.

Fission gases are not very soluble in the UO_2 matrix, and most of these fission products take up residence in the form of gas bubbles that become attached to grain boundaries. At very high burn-up the grain size becomes smaller in the RIM zone, and also leads to the formation of high number of micro-sized pores in which the fission gas is supposed to be contained; yet, these pores are not interconnected and do not significantly contribute to the gap inventory since the gap is closed. However, gas bubbles become interlinked along the boundaries in the fuel center, providing easier pathways for release to the gap. Hence fission product release to the gap is found to increase at high burn-up; a similar enhancement compared with UO_2 is seen for MOX fuel. These increases in release may require the modification of assumptions about gap activity that are used in safety analyses.

<u>Source term</u>

During and immediately following an accident, the part of the fission products inventory, released into the containment, potentially available for release to the environment is called the source term. In most countries, a severe-accident source term (associated with core melting) is defined deterministically to estimate radiological releases to the public for beyond design basis accidents. Source terms are also used in probabilistic risk assessments to estimate plant releases and accident consequences.

Source terms are based on measured releases from irradiated fuel, tested under accident conditions, in combination with assumptions or analyses of the effects of retention or enhancement during the course of an accident sequence. Source terms related to design basis accidents are calculated regularly, in conjunction with safety analyses for licensing of new fuel designs/core loading strategies, to evaluate radiological consequences. Thus, changes due to new design changes are accounted for, which may lead to changes in source term levels. However, the assumptions or analytical procedures themselves are not expected to change.

There are no safety criteria directly associated with source terms. Various assumptions are made for the analysis of accident scenarios and for retention effects, etc.; in part these are rooted in the basic reactor design philosophy, and can vary significantly between various regulatory frameworks. Although these differences are known, attempts to unite the analytical procedures and assumptions have not been very successful thus far. The effect on source terms from new design changes – especially from high fuel burn-up – is estimated as follows.

The main effects that could impact source terms as well as core melt progression at high burn-up are:

a) a modification in the amount of un-oxidized zirconium in the core,

b) embrittlement of the fuel cladding,

c) an increase in the release of fission gases from fuel pellets during normal operation,

d) fragmentation of fuel pellets,

e) a shift in the spectrum of fission products produced as plutonium fission becomes more important.

Effects (c), (d), and (e) could, in principle, also impact source terms for MOX fuel. It is unlikely that high burn-up will have a significant effect on source terms or core-melt progression. A similar statement can be made about MOX fuel: indeed, with respect to fission product release, the governing parameter is the local burn-up. Thus, because of the heterogeneity in burn-ups due to the initial presence of plutonium, MOX fuel may be seen as equivalent to a UO_2 fuel with higher burn-up. Also, the implementation of a revised source term could affect the dependence on new designs and materials. It is considered unlikely that new design changes or high burn-up will have a significant effect on source terms or core melt progression.

Burn-up

First, a short summary of the situation regarding the high burn-up issue (licensed burn-up limits, burn-up levels achieved today and expected burn-up extensions) is given. Licensed burn-up limits depend on the type of fuel and fuel vendor; licensed limits may refer to local (sometimes referred to as “peak pellet”) burn-up levels and/or rod average burn-up levels and/or assembly average burn-up levels.

Examples of licensed burn-up limits are as follows:

a) a maximum rod-average burn-up of 62 GWd/t for some fuels in the United States;

b) a generic limit for maximum fuel assembly average burn-up of 52 GWd/t exists in France for UO_2 and MOX fuel. MOX fuel was previously limited to 47 GWd/tM and 3 one-year cycles of insertion:

    a. Since 2007, the “MOX Parity” fuel management is now deployed in 21 French 900 MWe NPPs and allows 4 one-year cycles of insertion, and the same assembly discharge burnup for UO_2 and MOX fuel (enrichment of UO_2 is 3.7% U-235 and the average Pu content of the MOX assemblies is equivalent to 3.7% U-235).

c) the need for very high burn-up limits is somewhat alleviated in France by the use of fuel recycle technology. High burn-ups degrade the isotopic composition and “quality” of reprocessed fuels (MOX and enriched reprocessed uranium):

    a. A limit of 62 GWd/t has been licensed for the “GALICE” fuel management in France in the 1 300 MWe NPPs, which has been implemented in only one NPP, to gather in-reactor feedback.

    b. There is no plan to extend this type of high burn-up fuel management to other 1 300 MWe NPPs.

d) maximum assembly average burn-up is 57 GWd/t for the fuel type used in operating VVERs and 50 GWd/t for the fuel types used in operating BWRs in Finland;

e) generic limits for the maximum assembly average burn-up of 55 GWd/tU for UO_2 in PWRs and BWRs, 45 GWd/tHM for MOX in PWRs and 40 GWd/tHM for MOX in BWRs, respectively in Japan;

f) maximum assembly average burn-up of 65 GWd/t for PWRs and 53 GWd/t for BWRs, for some fuels in Germany;

g) maximum assembly average burn-up of 50 to 70 GWd/t, or maximum local burn-ups of 59 to 82 GWd/t for various different fuels in Switzerland.

Research showed that most current design limits could be retained when supported with data at the targeted burn-up levels. Criteria relating to the response of fuel to reactivity initiated accidents (RIAs) or to loss-of-coolant accidents (LOCAs) require a more complex evaluation. In recent years more information has become available on the behavior of highly burnt fuel. This has provided additional basis for the fuel/core operation for burn-up level up to those currently licensed. Clearly, one of the main benefits of high burn-up has been to decrease the fuel cycle cost. Another benefit has been the increased operational flexibility that high burn-ups allow:

a) The desire for higher burn-ups has been tempered in recent years in the United States as the migration to longer cycle lengths often results in the discharge of some fuel assemblies at lower burn-up.

b) That is, longer fuel cycle length reduces fuel utilization flexibility.

c) In addition, it is to be noted that the economic balance is more complicated to establish where fuel reprocessing is performed.

d) Indeed, when the burn-up increases, reprocessing may become more and more difficult.

Other considerations

Core management

The fuel cycle costs are an important part of the costs for plant operation. Utility strategies to reduce costs have increased the activity in the core management area; as a result of optimized core management, such as higher fuel discharge exposure, the loading strategies have changed. In the past, the loading strategy included the loading of fresh fuel into the center of the core and then, as a function of exposure, to move the fuel towards the edge of the core with each reload ("low leakage" loading pattern, or "in-out-out").

For this type of loading strategy the LHGR power history curves showed a monotonous decrease against fuel burn-up. Modern loading strategies use a smaller number of fresh reload bundles on account of the higher fuel discharge exposures. This leads to higher power peaking due to higher reactivity of those bundles.

Safety criteria, notably LHGR, SDM and DNB/CPR, must however still be met; as a consequence, fuel bundles with very high burn-up may now have to be loaded into a center of the core adjacent to fresh fuel bundles. This implies that reaching maximum fuel burn-up levels is no longer limited to those bundles at the core periphery.

Also other modern core management features such as the control cell core cycle design for BWRs (movement of only a few selected rods for reactivity control during the cycle) lead to having fuel with high burn-up in the core center. This situation may influence the behavior of the high burn-up fuel during transients/accidents.

As an example, during a small/medium size LOCA the cladding of the fresh fuel may collapse due to low cladding internal pressure and the high burn-up fuel may balloon due to high fission gas release during normal operation prior to the transient and during the transient itself. In the collapsed cladding case strong mechanical interaction between the fuel pellet and the cladding dominates internal oxidation of the cladding, and together with diffusion of the pellet material and fission gases into the cladding will cause fuel to fail; in the high burn-up fuel cladding ballooning, burst and double side steam oxidation are dominating mechanisms. Also during quenching and cooling down of the fuel the failure mechanisms are different between high burn-up and fresh fuel bundles.

The effect of the fuel failure mechanisms for high burn-up fuel may be enhanced by the larger reactivity (power) level in the adjacent fresh fuel; in return, the effects in highly burnt fuel could adversely affect the failure mechanisms in fresh fuel. Also, the different behavior of fresh and high burn-up fuel bundles has an impact of flow redistribution during the accident, which can challenge the fuel coolability criterion.

In summary, changes in core management do not directly upset safety limits or margins; as long as satisfactory modeling is available to describe the phenomena occurring in currently designed and operated cores, safety limits are not affected.

Mixed-oxide fuel

Some countries have chosen the option of reprocessing spent fuel.

Thus, contracts with reprocessing companies were put in place resulting in a certain quantity of fissile Pu, which can be used together with UO_2 to manufacture so called mixed-oxide fuel (MOX), as well as some amount of reprocessed uranium, which may be used as carrier material or blended with regular UO_2.

Also, the option of using weapons grade high enriched uranium and plutonium has been considered. Thus, MOX insertion is taking place (to date mainly in Europe) or is being planned in a number of countries, and is therefore of concern with respect to safety criteria. Various designs were and are being considered; presently the “all-MOX” type of design with the largest possible amount of Pu in the smallest possible number of assemblies appears economically to be the most attractive (with burnable absorber still blended with UO_2 only).

In general, the performance of MOX fuel is less characterized than for UO_2 fuel, especially at high burn-up. Experiments will continue to be needed to confirm the operational regimes in which MOX fuels are compliant with safety criteria, considering also that MOX performance can be affected by the fabrication route and by the total plutonium content in the fuel. Safety related effects of MOX (as compared to standard UO_2) insertion may be summarized as follows:

a) In general, a lower boron and control rod worth is to be expected due to the different isotopic and spectral characteristics.

b) For the same reason, a more negative Doppler and moderator temperature coefficient is generally observed.

c) Decay heat characteristics are slightly different (smaller short-term, but larger long-term effects).

This potentially results in a lower shut-down margin and faster transient response; radiologically, the different decay heat response will mitigate the accident response but aggravate long-term (e.g. storage) behavior. These effects are mainly counteracted by fuel and core design, analogous to the introduction of new fuel types. In particular, the design and subsequent safety analysis takes the specific characteristics of MOX into account, and ensures that the existing safety limits are met.

Although the assumption that safety criteria of UO_2 and MOX fuel are identical appears to be generally accepted, some questions on a possibly different behavior of MOX, especially at high burn-up, remain. The different MOX isotopes and pellet microstructure could lead to differences in e.g. the fission gas release characteristics, and thus indirectly affect criteria such as RIA. The review of the individual criteria should therefore include MOX fuel, as appropriate.

MOX fuel offers similar changes in fuel pellet material; UO_2 is replaced by PuO_2-UO_2 mixed oxide in which the PuO_2 content can vary from 2 to 13 wt% according to the rod position within the fuel assembly and the design criteria. For the case where MOX fuel has been used, the geometry, the dimensions, and the cladding material may be identical for UO_2 and MOX rods. In most countries the plutonium comes from recycling of “burnt” fuel; in addition, some effort has been applied to burning weapons-grade plutonium in commercial reactors in both the United States and Russia.

The introduction of new, advanced and/or MOX fuel can lead to a mixed core situation, i.e. fuel assemblies of different designs jointly reside in a core.

<u>Mixed assembly cores</u>

With the introduction of new fuel types (advanced designs, MOX, etc.) a “mixed core”, i.e. a core consisting of more than one particular design, automatically comes to pass. The fuel and core design must ensure that the newly introduced fuel is compatible with the residing fuel from a physics and thermal-hydraulic point of view; fuel and core safety limits are principally unchanged, but may have to be adapted to the mixed core situation. Each fuel type comes with a set of specific safety criteria, such as LHGR, oxidation or PCI. These limits are established by the respective fuel supplier, and must be met whether the core is mixed or not.

Other limits, such as the safety limit CPR or SDM, that relate to the entire core, must be analyzed by the responsible safety analysis engineer (usually at the fuel supplier).

The mixed core situation is thus basically covered by the safety analysis that the fuel and core design responsible suppliers perform. If utilities do not change fuel vendor, the various analyses are internally coherent; as long as the supplier design and monitoring methods are approved, no additional action is needed. If however more than one fuel vendor is involved, the utility must take appropriate action to ensure that the different methods and correlations do not carry over any inconsistencies or mismatches.

The mixed core situation is thus basically covered by an appropriate design and analysis, which should cover the following areas:

a) Neutronic and thermal-hydraulic compatibility, examples: local and global reactivity level, bundle flow characteristics (e.g. risk of flow starvation in neighboring bundles by low pressure drop for BWRs, or axial flow variations due to local flow redistribution for PWRs).

b) Development of safety limits, both for each individual fuel type and for the mixed core.

c) Safety analysis in which the mixed core features and incompatibilities are taken into account as appropriate.

Slow or incomplete control rod insertion

During the past few years, a malfunctioning rod scram was observed in several PWRs due to a slow or incomplete rod insertion. The changed scram reactivity may affect the fulfillment of the shutdown margin requirement, as well as the general transient/accident response. As a temporary measure, the effect of changed scram reactivity on SDM and transient response, based on the observed behavior, is taken into account for safety analysis and core design; the cycle specific design and reload safety analysis are adapted as appropriate.

Root cause analyses have shown that the mechanical properties of fuel assembly (leading to excessive fuel assembly distortion) and/or rod cluster control assembly (leading to rod swelling) are responsible; adjusting/improving the mechanical design is expected to lead to final resolution of this problem. The safety criteria themselves are thus considered unaffected.

Axial offset anomaly

When substantial CRUD build-up occurs in the upper part of a PWR core, especially in high-power assemblies, fission rates are reduced due to boron containing species ($LiBO_2$, Ni_2FeBO_5) being absorbed into the CRUD layer. As a result, the power distribution shifts towards the bottom of the core, causing a reduction in SDM and an increase in local peaking. During plant operation an anomalous, bottom peaked, power distribution is observed; should the power shift persist, burn-up effects will eventually reverse the power shift setting off a top peaked power distribution near the end of the cycle.

The bottom peaked power distribution will tend to reduce SDM, thereby causing deviations in the estimated critical position of control rods, and will also tend to increase local peaking. This phenomenon, called axial offset anomaly (AOA), has been observed mainly in high energy cores at several PWRs in the United States.

It is not expected that AOA will directly affect any of the fuel safety criteria. The actual numbers of some safety criteria, notably SDM, may change for those power plants (i.e. PWRs with high energy cores) affected.

Cladding diameter increase

For VVERs, it was observed experimentally that single event PCI criteria no longer protect against stress corrosion cracking beyond a creep and cyclic accumulation of plastic deformation of 0.4%. Thus, a design (strain) criterion limiting cladding diameter increase of 0.4% was put in place, covering creep and cyclic accumulation of plastic deformation.

For practical purposes, this design criterion is transformed into an operational recommendation to limit the number of significant power transients (including scram, start-ups, etc.). For western reactors, no such limit is defined; the requirement is considered to be covered by existing PCI criteria. On the other hand, the cladding diameter change during base irradiation (including gaseous swelling of the fuel pellets and creep of the cladding) has to remain below 1% strain in western reactors. This limit has been set up to be maintained during the whole irradiation, to consider the hydraulic section of the fuel assembly channels and thus the DNBR margins.

Cladding elongation

Following a general fuel design requirement, the fundamental mechanical and hydraulic functions of the assembly shall not be impaired due to irradiation growth of fuel rods and channel; in particular, the fuel assembly shall give sufficient space for differential rod growth to occur without it becoming restrictive. For western reactors, no explicit elongation (axial growth) design limits are defined. The vendor design process includes verification of the general design requirement against values obtained from experimental data (in-pile and out-of-pile) with suitable uncertainty analysis.

Radial peaking factors

The radial peaking (F_r for WWERs or enthalpy rise hot-channel factor $F_{\Delta h}$ for PWRs) is sometimes used as a limit to prevent DNB and for WWERs also to prevent reaching saturation temperature of the coolant on the assembly outlet under normal operating conditions and AOOs. A radial peaking factor (K_r or F_{xy}) is derived by including the uncertainties in measurements, design methods and fabrication tolerances; this becomes one of the limits for reload design purposes. The limit is also verified during operation with the use of core monitoring programs.

For most western reactors, the radial peaking is employed to indirectly verify the DNBR criterion not only for core design but also during plant operation; for this reason, it is sometimes specified in the technical specifications of the plants.

3D peaking factor

A total peaking or “hot spot” factor is defined for design purposes to limit local power peaking during normal operation. The limit is also verified during operation with the use of core monitoring programs.

For western reactors and for VVERs, the three-dimensional (3-D) peaking factor is employed to indirectly verify LHGR as well as the DNBR operating limit not only for core design but also during plant operation; for this reason, it is sometimes specified in the technical specifications of the plants.

Special attention must be paid to WWER-440 fuel due to potentially large local power peaking (up to 70%) in the fuel surrounding the connecting part between the absorber and the fuel follower of the control rod. Recently Hf containing absorber segments have been introduced to handle this problem.

Cladding stability

Cladding stability limits are defined to prevent cladding collapse due to ovalization. For western reactors these are normally design limits, constraining elastic and plastic deformation, which are verified analytically. The maximum as built fuel tube ovality is typically defined in the tubing specification and 100% inspected to ensure analytical basis is valid.

For VVERs, deformation is also verified against design limits and ovality is traced analytically during the expected lifetime of the fuel rod. As the integrity of the plant primary circuit is checked every four years at a higher than normal operating pressure, it must also be verified that the cladding does not collapse during this test. Thus, an ultimate pressure is calculated at which the cladding would collapse and compared against the pressure operating limit associated with such tests; if the ultimate pressure is below this operating limit, the fuel design must be changed.

Conclusions

In considering the fuel criteria discussed in this chapter, some are of greater interest to the nuclear industry (PCI) than to the regulatory authorities, and vice versa (CHF). In the same manner, some criteria are well established and well documented. Other criteria are evolving and may be impacted by emerging issues or the better understanding of these issues