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Nuclear Safety Parameters of Mixed TRIGA cores

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1 Introduction

Appearance of powerful yet inexpensive small computers stimulated development and applications of reactor calculations also at small reseach reactor centers. Experience shows that more and more research reactor users apply reactor calculations to their work, however, mainly for fuel management purposes. More demanding aspects of reactor calculations such as analysis of reactor experiments, optimization of irradiation facilities and safety analysis are usually avoided partly due to lack of computer codes or insufficient computer capacity but partly also due to insufficient experience of the user. On the other hand strong interest is observed for nuclear safety analysis calculations. Many research reactor users are faced with the situation that requires revision of the safety analysis report, e.g. core conversion, mixed core operation, power upgrading, etc. The purpose of this lecture is to explain principles and demonstrate reactor calculations of the most important safety related core parameters using computer codes which are available to typical research reactor user.

Two reactor parameters are particularly important from the aspect of safety analysis, i.e., power peaking factor and fuel temperature reactivity coefficient. They are used for determining limiting conditions of normal operation as well as in analysis of postulated transients. They strongly depend on the core configuration and on the fuel type. Results of generic calculations or results found in literature are for this reason not applicable in many practical cases and explicit reactor calculations have to be performed. However, certain requirements which are not typical for reactor calculations in general but which are standard in safety analysis, must be respected: computer codes must be tested and experimentally verified, results of calculations must be evaluated and calculational error must be estimated, physical models must include conservative assumptions. Detailed understanding of the problem as well as practical experience in reactor calculations is required.

Power peaking calculations are treated for several typical TRIGA reactor core configurations: uniform and mixed core, with and without central irradiation channel etc. Influence of the water gaps in the core is analysed. Both, steady state and pulse mode operation are considered for several cases. Fuel temperature reactivity coefficient is calculated primarily for unit-cell or uniform homogeneous reactor geometry, while temperature and power defects are treated also for mixed cores. Three types of fuel elements are considered: standard (8.5 w/o and 12 w/o U), LEU and FLIP (Table I). Computer code WIMS-D/4 [1] is used for unit cell transport calculations. TRIGAP [2] and SIXTUS [3] are used for multigroup diffusion calculations in 1-D cylindrical and 2-D hexagonal geometry, corresponding to both most common TRIGA reactor core configurations. It is beyond the scope of this lecture to describe the computer codes in details. Only the properties relevant to

power peaking or temperature reactivity coefficient calculations are discussed in due course.

Table I. Main physical characteristics of different TRIGA fuel element types

Common

fuel

material	U ZrHx
inner diameter	.64 cm
outer diameter	3.64 cm
length	38.10 cm

cladding

material	stainless steel
outer diameter	3.75 cm
thickness	.06 cm

Specific

	standard		LEU	FLIP
U concent. [w/o]	8.5	11.7	20.1	8.5
weight UZrHx, [g]	2245	2360	2462	2258
enrichment	19.9	19.9	19.8	70
H:Zr	1.66	1.65	1.57	1.65
weight U-235, [g]	37.9	55.0	97.8	134.2
Er concent. [w/o]	-	-	.43	1.53

2 Power peaking factors

2.1 Steady state operation

Two power peaking factors are important for steady state operation:

- hot rod power peaking factor f_{HR} and
- axial power peaking factor f_Z.

They determine maximum total power released by one fuel element as well as its axial peaking value which are used as parameters in thermal hydraulic analysis (maximum fuel temperature calculation, DNBR calculation).

Hot rod power peaking factor is defined as the ratio between the maximum power released by one fuel element (rod) P_{ROD} and average power per element in the core P_{CORE},

$$f_{HR} = \frac{(P_{ROD})_{MAX}}{P_{CORE}}, \tag{1}$$

According to this definition

$$P_{CORE} = P/N_{EL}, \tag{2}$$

where P is total reactor power (e.g. 1 MW) and N_{EL} is number of elements in the core. Taking into account that all types of fuel elements have the same volume of fissionable material V_{EL}, the definition of f_{HR} (1) applies also to the ratio between average power density $\bar{p}_{ROD}$ of the hot rod and core average power density $\bar{p}_{CORE}$

$$f_{HR} = \frac{(P_{ROD})_{MAX}/V}{P_{CORE}/V} = \frac{(\bar{p}_{ROD})_{MAX}}{\bar{p}_{CORE}}. \tag{3}$$

This definition is more appropriate for pulse analysis as it will be shown later, while for steady state conditions (1) is more convenient.

Axial power peaking factor is defined as the ratio between maximum and average axial linear power density, $p(z)$ and $\bar{p}_Z$, respectively,

$$f_Z = \frac{(p(z))_{MAX}}{\bar{p}_Z}. \tag{4}$$

In principle f_Z may vary from fuel element to fuel element. Strict definition requires that is is taken at radial location in the core where it has maximum value. However, differences between axial power distributions in different fuel elements in the same core are small because TRIGA reactor is very small (short) and axial power distributions are hard. Calculations and experiments show almost analytical "chopped cosine" shape of axial power distributions with peak to average value 1.25 for all types of fuel and for all practical core configurations. This value may be taken for f_Z in practical safety evaluations and needn't be explicitely calculated from case to case [1].

In contrast to axial power peaking factor, hot rod factor significantly depends on the loading pattern and other core characteristics as can be seen from Table II where results of reactor calculations are summarised for several hypothetical core configurations. Influence of different heterogeneities on the peaking factor can be observed. Core geometry is hexagonal (Fig. 1) and only fresh standard 12 w/o U fuel is used. Reflector is water. Calculations are performed in two-group 1-D approximation using program SIXTUS. Group constants for unit cells are calculated using WIMS-D/4 with TRIGA library [4]. Irradiation channel (IC) is considered to be empty aluminium tube with the same radius as fuel element (dry) or filled with water (wet).

[1] WIMS code can be directly used for axial power distribution calculations (plate geometry option).

Table II. f_{HR} for different core configurations (hexagonal array, water reflector, fresh standard 12 w/o U fuel only)

core description	f_{HR}	position (ring)
a. fuel in A, B, C, D	1.37	A
b. fuel in A, B, C, D, E, F	1.53	A
c. fuel in B, C, D, IC in A, dry	1.37	B
d. fuel in B, C, D, E, F, IC in A, dry	1.60	B
e. fuel in B, C, D, IC in A, wet	1.39	B
f. fuel in B, C, D, E, F, IC in A, wet	1.65	B

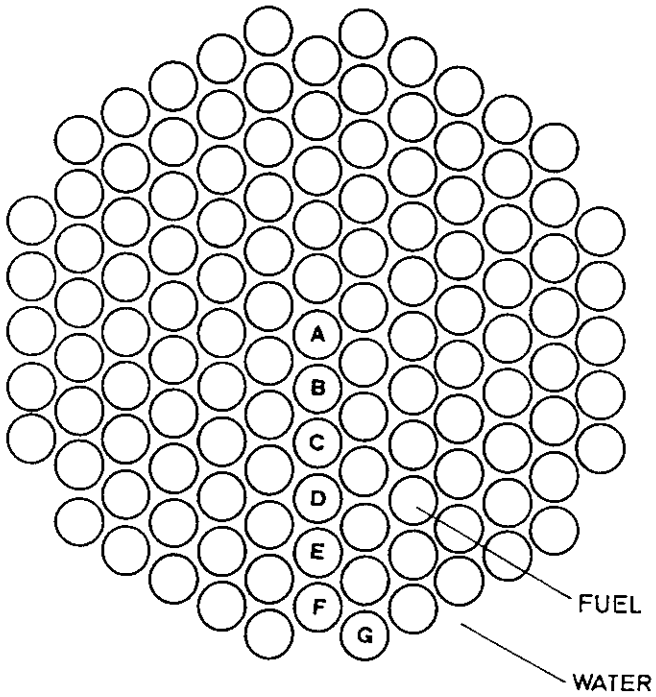

Figure 1: Schematic diagram of TRIGA core in hexagonal geometry

We first note from results in Table II that the peaking is always found in the centre of the core. This is always true in uniform cores while in mixed cores it is found also in outer rings depending on the loading scheme. Next we note that f_{HR} depends on the core size: it is decreased if core size is reduced. This can be explained by the relatively stronger effect of reflector in small core. However, the core in question is extremely small and feasible in practice only with fresh fuel and zero power. In all practical cases $f_{HR} \approx 1.53$ is more realistic estimate for f_{HR} in cores without central irradiation channel.

We see that f_{HR} is increased in all practical cases if central irradiation channel is used. Increase is $\approx$ 5 % if it is dry and $\approx$ 10 % if filled with water. In such case reasonable and conservative estimates for f_{HR} are 1.60 and 1.65, respectively. These values are typical for uniform TRIGA cores and are usually used as generic values in thermal hydraulic analysis. However, the purpose of this lecture is not to derive general results, they are given only to get feeling about realistic values of f_{HR}. Our purpose is to show that f_{HR} strongly depends on the core configuration and must be calculated from case to case. This is particularly true for mixed cores. No general rules or estimates for f_{HR} can be derived perhaps only very general but not strict ones, such as that f_{HR} is usually found on fuel elements with higher uranium content (FLIP, LEU) and that it can considerably exceed typical value for uniform core ($\sim$ 1.6), if high uranium fuel is put into the central rings. The only general rule that strictly applies is that reliable estimate for mixed core f_{HR} can be obtained only by detailed reactor calculation.

An example of such calculation is presented in Fig. 2 [5]. The reactor is TRIGA Mark II with hexagonal grid and water reflector (see Fig. 1). Central irradiation channel is in A ring. Core is loaded with fresh 8.5 w/o standard fuel from rings B to G. No water gaps or other heterogeneities are present. It is assumed that fueled follower control rods with 8.5 w/o standard fuel are used and that they are in completely withdrawn position. It means that all rings from B to G are completely filled with fuel. f_{HR} for such case is 1.67 as can be seen from Fig. 2. Then standard fuel elements in one of the rings are replaced by LEU elements: from one to all elements in the ring. This is done for all rings. Note that LEU fuel is always only in one ring but position of this ring is varying. We can see from Fig. 2. that f_{HR} is maximal when only one LEU is inserted in B ring. The power of this element would be $\approx$ 2.4 times higher than the core average! If thermal-hydraulic design of the core was performed assuming uniform core $f_{HR} \approx 1.6$ at full power this loading pattern would extremely violate design bases safety limits if reactor power would not be reduced. Of course such loading is not acceptable. If more then one LEUs are inserted in B ring f_{HR} is reduced but not significantly. Approximately the same conclusions apply also if LEUs are inserted in C ring. Only when they are inserted in D (note: all other rings are filled with standard fuel), f_{HR} approaches but still exceeds uniform core limit. However, f_{HR} is not found in the centre (B ring) but at LEU fuel elements in D ring. It is also interesting to note that when LEUs are inserted in ring E, F or G, f_{HR} becomes lower than in uniform core, because higher power at the core periphery tends to flatten the overall reactor power distribution.

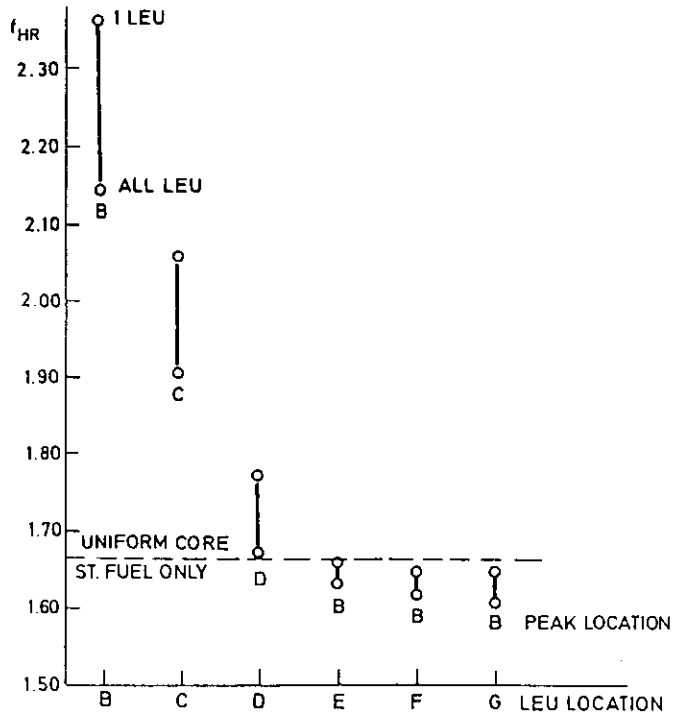

Figure 2: f_{HR} as a function of LEU location in mixed core with 8.5 w/o standard fuel. Vertical lines correspond to two extreme possibilities: only one LEU in mixed ring or all position in this ring filled with LEU.

2.2 Pulse operation and transients

f_{HR} and f_Z completely determine power distribution in the core for temperature and hydraulic calculations of steady state conditions. However, for calculations of fuel and cladding temperature distribution an assumption must be made also about the radial power distribution in the fuel element.

Radial power density distribution in the fuel element is presented for different types of fuel elements in Fig. 3. It was calculated using program WIMS-D/4 in unit cell geometry. Power density is approximately proportional to thermal flux distribution which reaches its maximum in water around the element and decreases in the fuel element due to much higher absorption in fuel than in water. However, temperature calculations show that power density distribution doesn't significantly influence the radial temperature distribution which has approximately parabolic shape with maximum in the centre. Consequently, the radial power distribution is assumed to be constant (independent of radius) in temperature calculations.

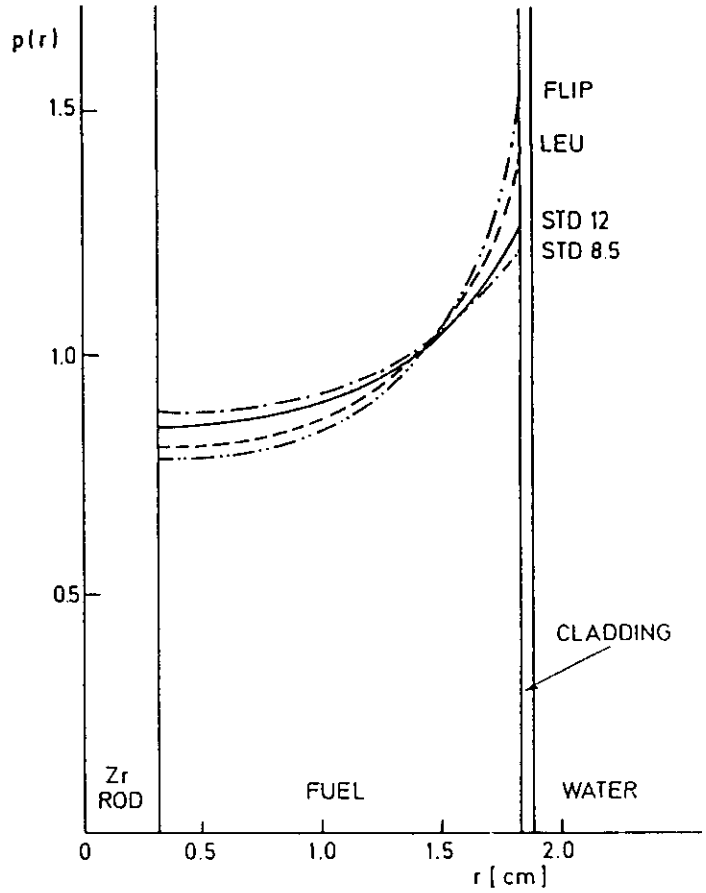

Figure 3: Relative radial power density distribution p(r) for different types of fuel elements

This assumption is conservative with respect to the maximum temperature in the fuel element for stationary conditions. For pulsing it is more conservative to assume that the temperature radial distribution is proportional to the power density distribution as the pulse is so short (10 - 50 ms) that heat is practically not transferred from the point where it is released before temperature reaches maximum value. This is called adiabatic approximation. Released energy and temperature immediately after the pulse are proportional to power distribution with peaking at outer radius of fuel material (see Fig. 3). This peaking is called rod power peaking factor f_R defined as peak to average radial power density $p(r)$ in the fuel element

$$f_R = \frac{(p(r))_{MAX}}{\overline{p}_R}. \tag{5}$$

However the radial power density needn't be radially symmetrical and p is not a function of r only. If the fuel element is found at a location with high thermal flux gradient (e.g. near water gaps, irradiation channel or at the boundary between zones in mixed core), the radial power distribution inside rod is tilted in the direction of the flux gradient. For this reason it is convenient to split f_R into two subfactors

$$f_R = f_R^R \times f_R^G \tag{6}$$

where f_R^R is called radial rod power peaking factor and f_R^G power gradient rod power peaking factor. f_R^R is defined by eq. (5) assuming symmetry boundary conditions at the unit cell boundary. It means that the fuel rod is found in periodic array of fuel rods and that there are no water gaps or other irregularities in the core. f_R^G is defined as the ratio between maximum and asymptotic power density ($r \to \infty$) around an irregularity in the core with homogenized fuel unit cells (See Fig. 4).

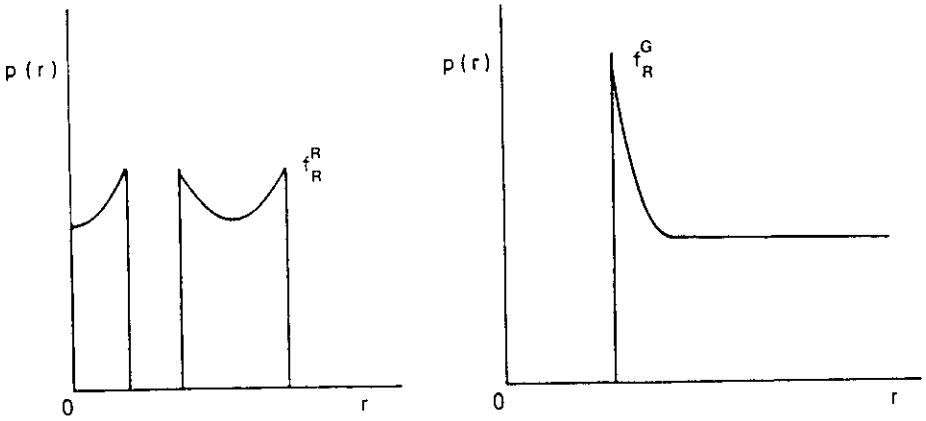

Figure 4: To the definition of f_R^R and f_R^G

f_R^R in uniform core depends mainly on the fuel element type as can be seen from Fig. 3. Its value is between 1.25 for standard and 1.55 for FLIP fuel. In a mixed core f_R^R can be even higher. The most conservative case of mixed core f_R^R is presented in Fig. 5., i.e. one 12 w/o standard, LEU or FLIP element in the sea of 8.5 w/o standard elements. We see that f_R^R is significantly increased up to 2.5 for FLIP due to high thermal flux in surrounding standard fuel.

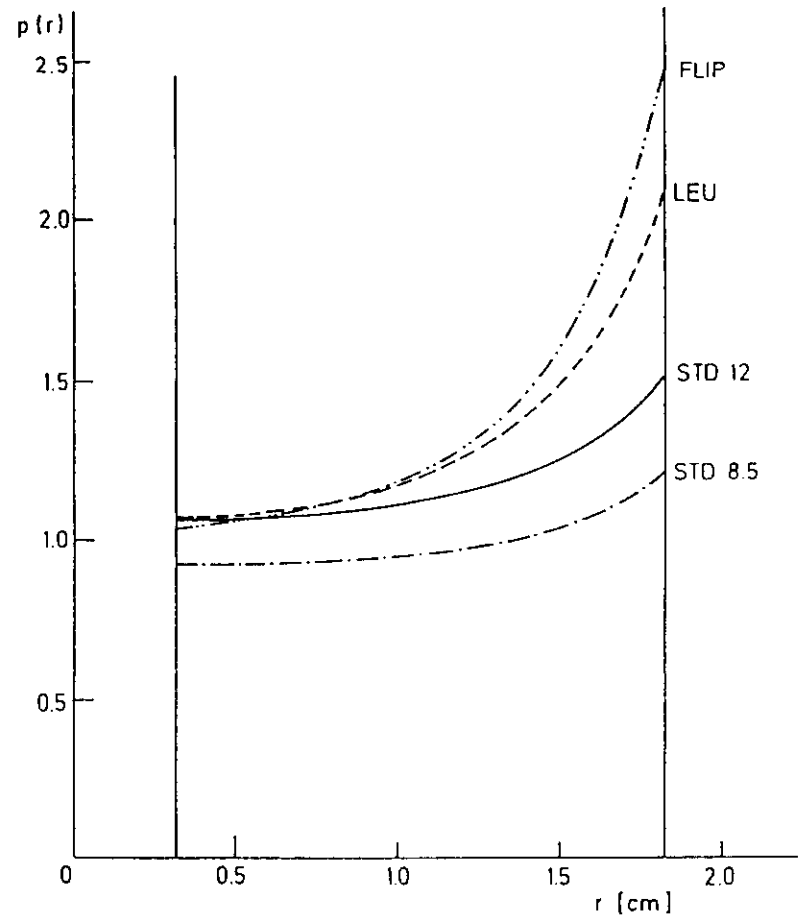

Figure 5: Relative radial power density distribution $p(r)$ for different fuel elements in infinite lattice of 8.5 w/o standard fuel elements

f_R^G is per definition equal to one in a compact core without water gaps or irradiation channels. However, practical core configurations always include irradiation facilities in the core particularly in the centre where flux is maximal. f_R^G is therefore very important especially because it becomes significantly greater than one when big irradiation channels or flux traps are applied. Figs. 6 and 7 show radial power distribution around a water gap or irradiation channel surrounded by 12 w/o standard fuel. The size of the gap corresponds to one (Fig. 6) or three (Fig. 7) unit cells. Surrounding fuel unit cells are homogenized. We see that gradient power peaking may become even more important than f_R^R if big water gaps or flux traps are used

in the core having in mind also that in case of FLIP or LEU fuel f_R^G becomes even higher.

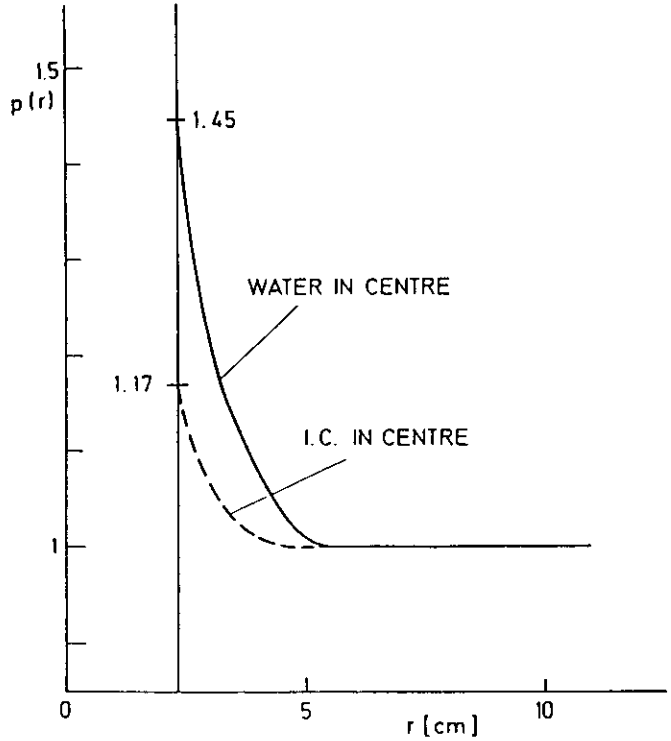

Figure 6: Radial power density distribution $p(r)$ around a water gap or irradiation channel. Size of the gap is one unit cell.

It can be also observed that the peaking is extremely localized and that water gap influences only its first neighbours. Even there the average power of the element is not significantly affected. The effect can not be treated properly by diffusion calculations of the entire reactor even if finite differences and pin by pin approximation were used. Only rough approximation is obtained for f_R^G while f_R^R can not be calculated with diffusion code at all. f_R^G and f_R^R can be calculated only in multigroup transport approximation. The geometry model must be as close to reality as possible. WIMS-D/4 is used for calculations of results presented in Figs. 5 - 7. One dimensional unit cell geometry is used (hint: use OPTION 0 and NU FISSION x FLUX DENS. x VOLUME results, last line). Two dimensional approximation (PIJ) could be used in more complicated geometries.

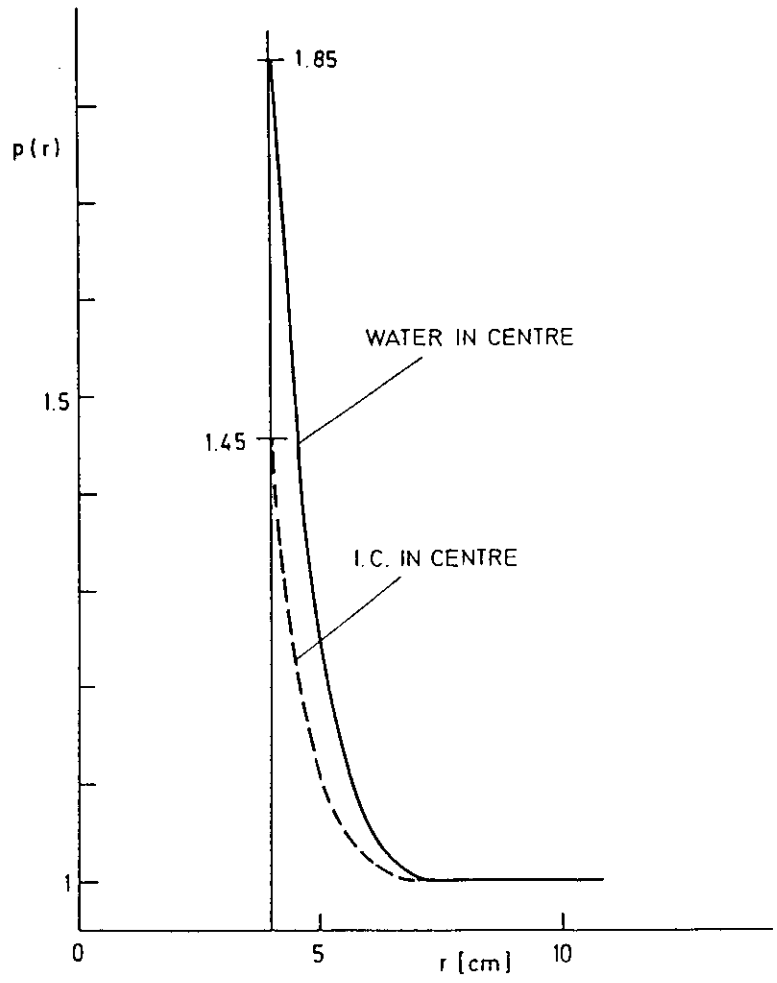

Figure 7. Radial power density distribution $p(r)$ around a water gap or irradiation channel (60 % void, 40 % water). Size of the gap is 3 unit cells.

Let us now for example calculate total power density peaking factor for pulsing f_T for a uniform 12 w/o standard fuel core with dry irradiation channel in the centre:

$$f_T = f_{HR} \cdot f_Z \cdot f_R^R \cdot f_R^G = \quad (7)$$
$$= 1.60 \cdot 1.25 \cdot 1.25 \cdot 1.17 = 3.0$$

Results are taken from Table II and Figs. 3 and 6. Now let us suppose that one element in B ring is replaced by FLIP and that irradiation channel is filled with water or another good moderator:

$$f_T = 1.65 \cdot 1.25 \cdot 2.5 \cdot 1.45 = 7.5 \tag{8}$$

The value is extremely high telling that the peak temperature increase in FLIP would be at least 7 times higher than the core average after the pulse. And even this estimate is not enough conservative since f_R^G is taken from Fig. 5 for standard element. Note also that f_T was calculated rather using f_{HR} for uniform B ring (Table II) and f_R^R for FLIP in uniform core of standard fuel (Fig. 5) than f_{HR} for one FLIP in B (which should still have to be calculated) and f_R^R from Fig. 3. Both ways are equivalent for f_T. However, power of FLIP in B is not only 1.65 times greater than core average as could be misunderstood from (8). It can be roughly estimated from Figs. 3 and 5 taking 1.65 for the ring average f_{HR}:

$$f_{HR}(FLIP) = \frac{2 \cdot 5}{1 \cdot 5} \times 1.65 = 2.75. \tag{9}$$

However, this in only a rough estimate because it was our aim to calculate f_T and not f_{HR}. Explicit diffusion calculations like in case of LEUs in Fig. 2 should be performed for more precise estimate of f_{HR}.

It is neither our intention to analyze this hypothetical but not completely improbable case nor to enter the problem of maximum temperature calculation. It is only our purpose to show that pulse power peaking calculations must be considered from case to case with extreme care in order to obtain reliable and conservative results for the safety analysis and in final consequence, to avoid fuel damages. This is particularly true for mixed cores where no practical and conservative generic estimates can be produced for the power peaking factors.

3 Fuel temperature reactivity coefficient

Fuel temperature reactivity coefficient (α_f) is important for determining stationary and transient properties of the reactor: power defect and shut-down margin, pulse shape and energy, etc. In many practical cases, α_f is determined on the basis of data found in literature. Calculations are avoided because the fuel management codes, which are commonly used by research reactor operators, are not appropriate for this kind of problems. However, in some cases, (e.g. mixed core operation), α_f can not be determined without calculations, because it depends on type, number and loading pattern of fuel elements in the core.

3.1 Calculations of α_f in unit-cell approximation

The physical mechanism of fuel temperature reactivity effect is explained in ref. [6]. Two main effects contribute to the fuel temperature reactivity coefficient: Doppler broadening and spectrum hardening.

Doppler effect is common to all reactors containing U-238. The lattice-cell computer codes (e.g. WIMS) are provided with evaluated resonance cross-sections tabulated in dependence of fuel temperature and sigma potential. They do not depend significantly on the fuel type and can therefore be used also for TRIGA calculations. Lattice effects in resonance region (Dancoff and Bell corrections) are small in almost homogeneous reactor like TRIGA. Also their temperature dependence is weak. It can be concluded that WIMS is appropriate for modeling Doppler effect in TRIGA fuel without modifications.

However, Doppler broadening contributes only less than half of the total fuel temperature reactivity effect in standard TRIGA fuel. The rest contributes non-uniform hardening of the neutron spectrum in fuel and water. Increase of fuel temperature results in shift and deformation of Maxwellian spectrum in fuel while spectrum in water is only slightly affected, because the water temperature remains almost constant. As a consequence, reaction rate in fuel is decreased, while it remains practically constant in non-fissionable part of the unit cell (water, fuel cladding). Ratio between fission and absorption reaction rates is reduced in average over the entire unit cell. Multiplication factor is reduced and spectrum hardening effect is negative.

However, magnitude and temperature dependence of the effect can not be predicted without taking into account specific properties of the scattering cross section of hydrogen bound in zirconium lattice. Calculations show, that α_f absolute value will be extremely overestimated, if the hydrogen scattering cross-section in fuel is taken the same as for hydrogen bound in water (Fig. 8).

This is especially true for low temperatures, because differences between scattering cross-section of H in Zr H_z and H_2O increase for low energies. With increasing temperature the differences gradually disappear since the thermal spectrum shifts to the energies above binding energies of H in Zr lattice or H_2O molecule and the cross-sections approach to values for the H atom.

As described above, the use of proper scattering cross-sections for H in Zr H_z is essential for calculations of α_f in TRIGA fuel. Original WIMS library does not contain such cross-sections. In this case they were processed from ENDFB-3 files using FLANGE code for thermal energies and inserted into the WIMS library as material 5001 [4]. Scattering matrices are tabulated at several temperatures (296^oK, 400^oK, 600^oK and 1000^oK) like other H cross-sections in original WIMS library. Temperature dependence of α_f for standard 8.5 w/o TRIGA fuel calculated with WIMS-D/4 using 5001 data for H is presented in Fig. 9.

It can be observed, that α_f shows discontinueities at the tabulating temperatures but general trend or average values over the temperature intervals agree very well with references. The discontinuites could be reduced by using more fine temperature tabulation. However, this was not performed mainly for practical reasons and also because α_f is usually used in integral form (as temperature or power defect), where the discontinueities disappear (Fig. 10).

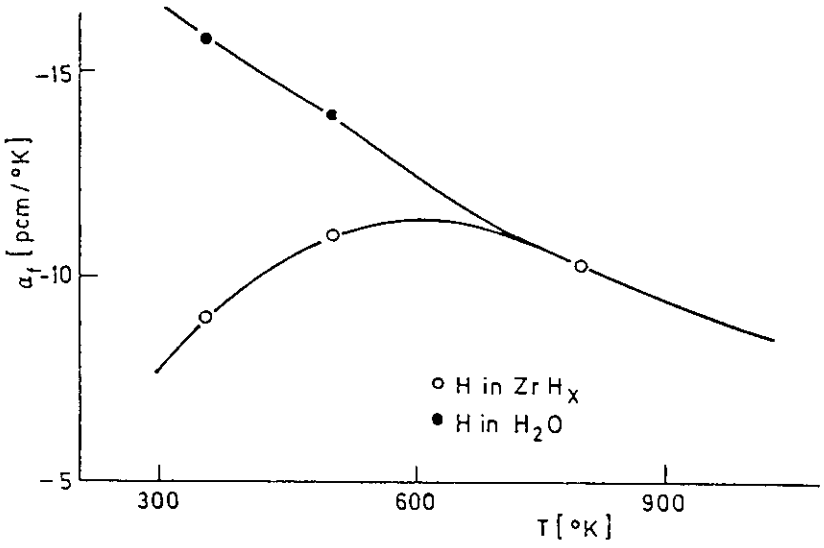

Figure 8: α_f calculated with WIMS-D/4 using cross-sections for H in fuel as
a.) bound in Zr lattice
b.) bound in water molecule

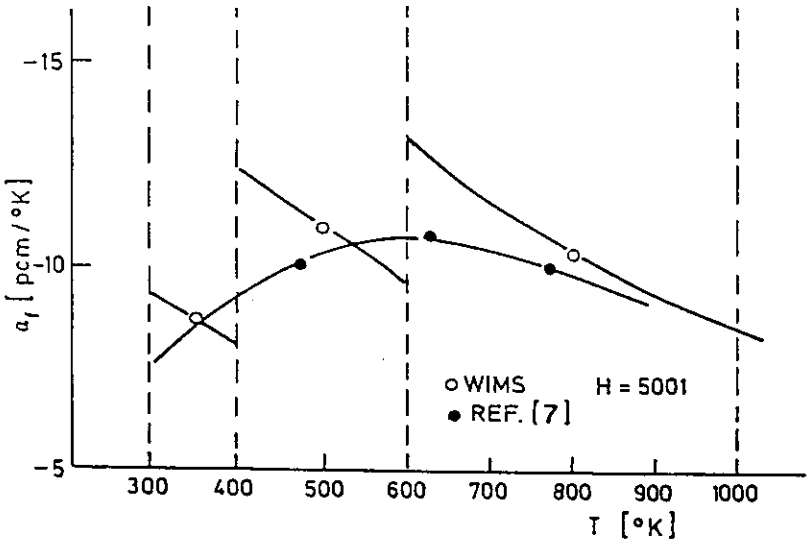

Figure 9: Temperature dependence of α_f calculated with WIMS-D/4 for fresh standard 8.5 w/o TRIGA fuel

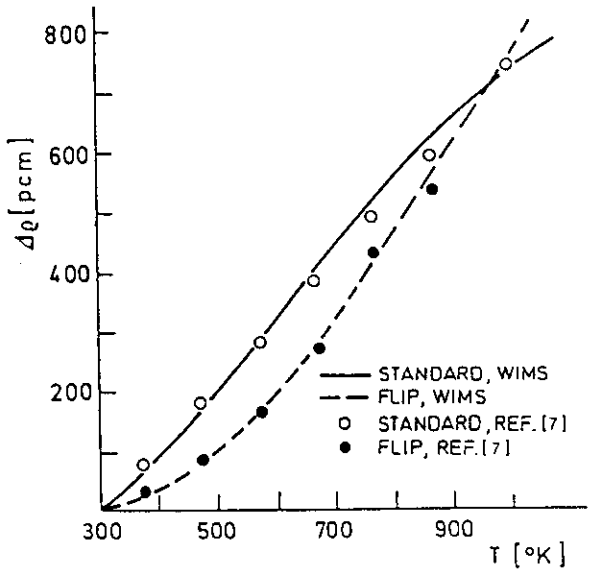

Figure 10: Temperature reactivity defect as a function of temperature for fresh standard and FLIP fuel

In case of TRIGA fuel containing burnable poison erbium (FLIP, LEU), the negative temperature effect on reactivity is even amplified, because the temperature shift of thermal spectrum enhances absorption of low energy Er resonances. Effect is strong ($\sim 1/3$ of α_f for FLIP) and may not be neglected in practical calculations. Like H in Zr H_x also Er-166 and 167 were not included in original WIMS library and had to be processed from ENDFB-4 data using FEDGROUP code. The final results are in good agreement with references. They are presented for standard, LEU and FLIP fuel in Figs. 11, 12, 13 and 14. Comparison to reference values shows that WIMSD-4 may be used for temperature reactivity effect calculations for all main TRIGA fuel types, provided that the appropriate cross-section library was used.

Figs. 13 and 14 suggest, that burnable poison contributes a positive effect to α_f since it is smaller for FLIP or LEU than for standard fuel at least for temperatures below $600°K$. However this is only apparently in contradiction with explanation above. Small absolute value of α_f for FLIP is result of high enrichment, as can be seen from Fig. 15. Explanation is: as fuel absorption increases proportionally to enrichment, relative contribution of temperature induced spectrum hardening to total absorption in the cell becomes smaller. In case of LEU this effect is not due to high enrichment but high U concentration what is practically the same.

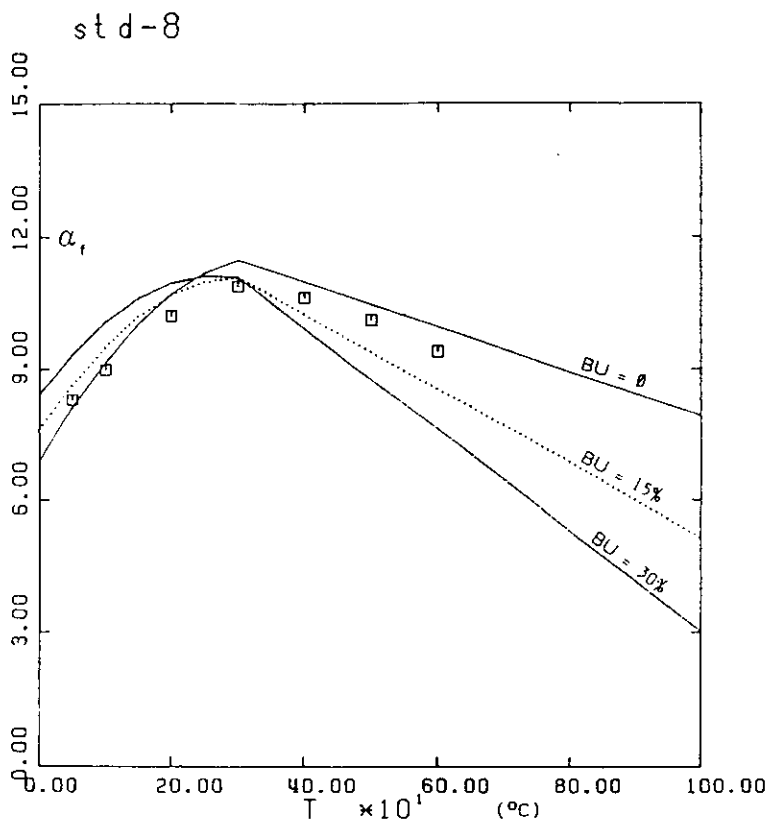

Figure 11: α_f in pcm/°C for 8.5 w/o standard fuel as a function of temperature and burnup (lines) in comparison to reference [7], dots

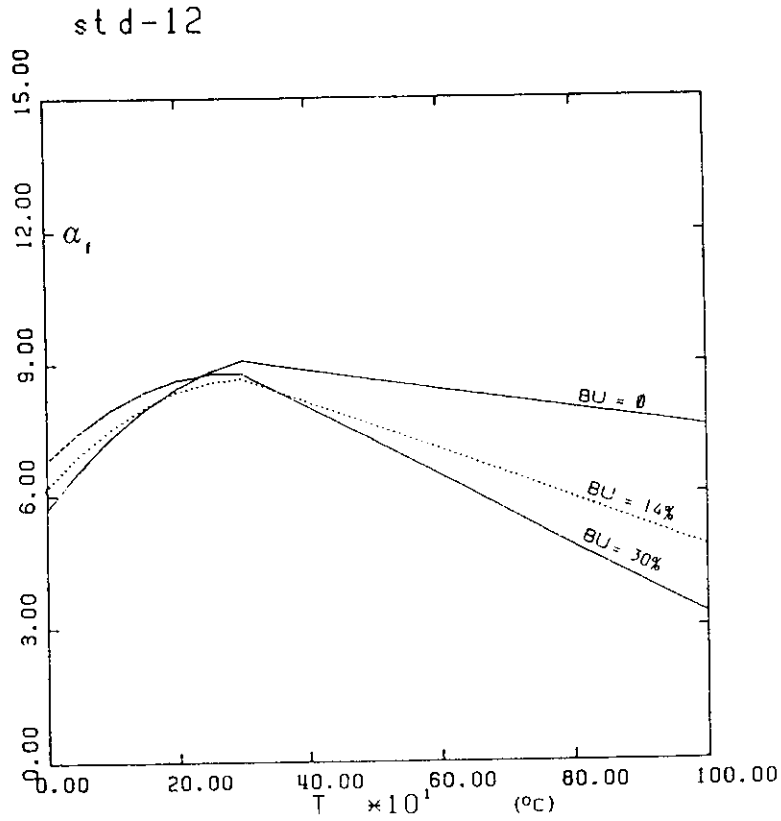

Figure 12: α_f in pcm/°C for 12 w/o standard fuel as a function of temperature and burn-up

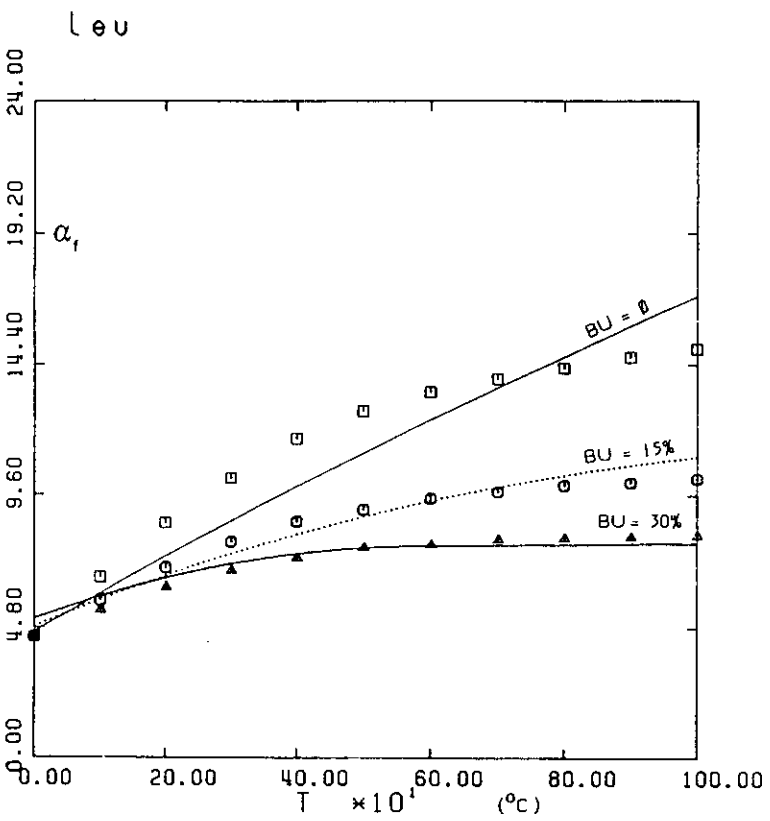

Figure 13: α_f in pcm/°C for LEU fuel as a function of temperature and burn-up (lines) in comparison to reference [7] (dots)

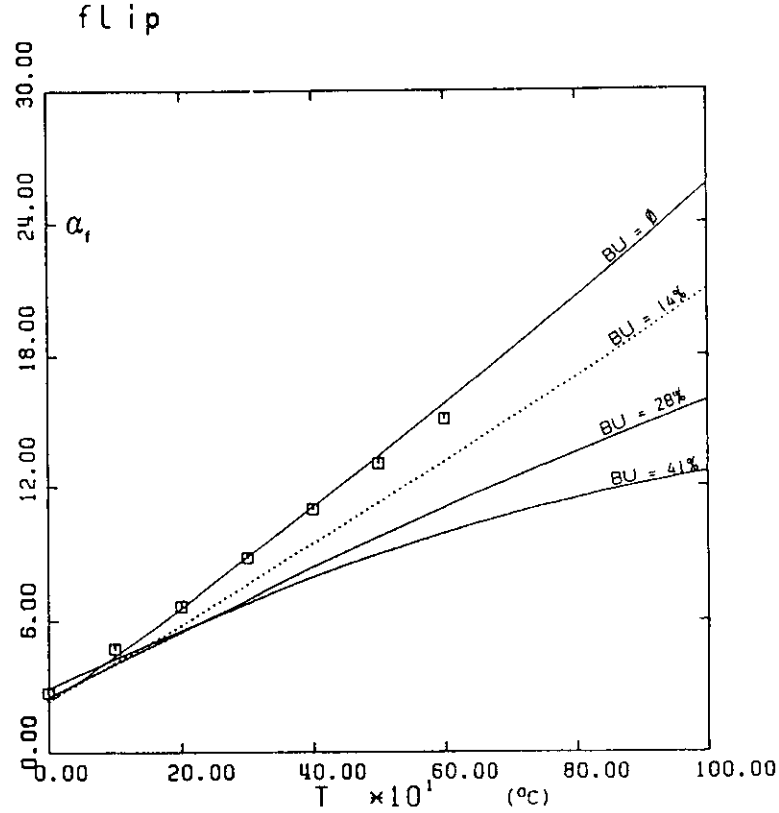

Figure 14: α_f in pcm/°C for FLIP fuel as a function of temperature and burn-up (lines) in comparison to reference [7] (dots)

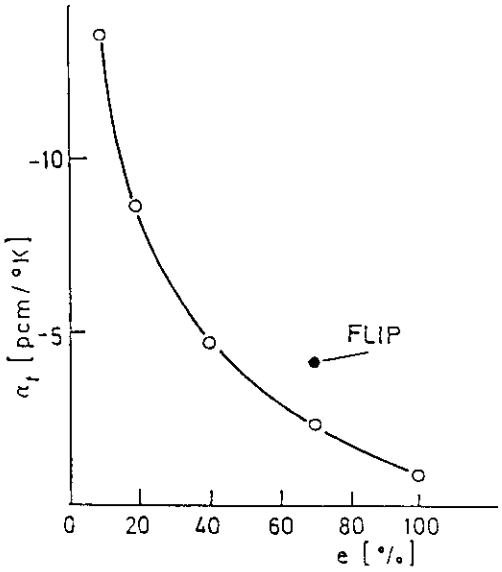

Figure 15: α_f as a function of enrichment for standard 8.5 w/o fuel, (at $20°C$)

3.2 Calculation of mixed core α_f

Definition of α_f for a mixed core is not convenient for power operating conditions, because fuel temperature is not constant (independent of position in the core) and an average over the core must be found. For the same reason α_f can not be measured with confidence. Instead of α_f, power reactivity coefficient and defect are used in practice as they can be directly compared to measurements.

Power defect of a reactor with uniform core depends practically only on the number and type of fuel elements. If number of fuel elements is increased, average specific power released by one element proportionally decreases and core average temperature is decreased as well (at constant reactor power). Power defect is inversely proportional to the number of elements if temperature and average specific power is sufficiently low (l.t. 5 kW/el. for natural convection), where the relation between fuel temperature and specific power is practically linear. This is true also because α_f is the same for all fuel elements in the core.

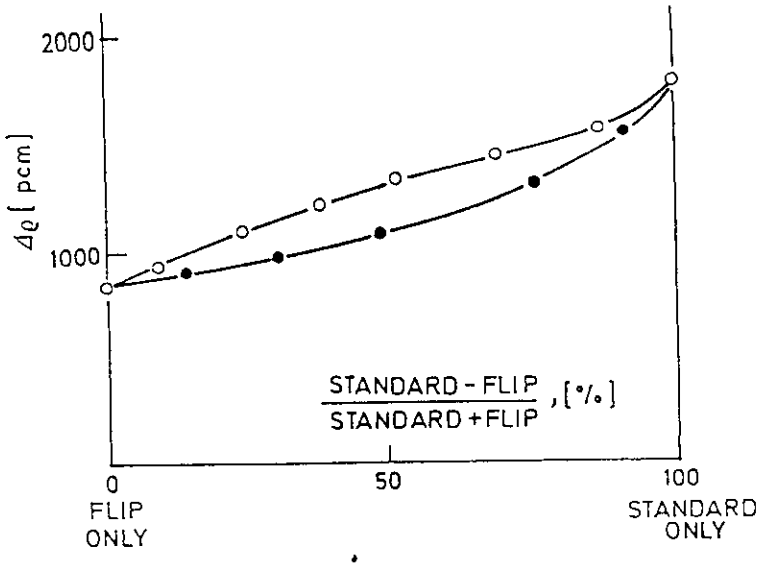

Figure 16: Power defect $\Delta\rho$ of a mixed core with 66 elements as a function of its composition for two loading strategies
upper curve: standard elements 8.5 w/o U in inner rings, FLIPs in outer
lower curve: FLIP elements in inner rings, standard 8.5 w/o U elements in outer

In mixed cores, α_f is different for different elements. α_f of the core and corresponding power defect is weighted average of α_f values for all fuel elements. The weighting function depends on the location in the core: inner rings are more important than outer and power defect of the mixed core is closer to the value for the fuel in the centre than for the fuel at the periphery. Effective temperature coefficient and power defect depend not only on the number of fuel elements of different types but also on their loading pattern. The effect is presented in Fig. 16 for a mixed cylindrical core operating at 390 kW. The core consists of standard and FLIP elements in rings from B to F. Numbers of elements in both regions are varied, but the total number in the core is constant. Two curves correspond to two different loading strategies. Upper curve corresponds to the loading schemes, where standard elements are all loaded in inner rings and FLIPs in outer rings . Lower curve corresponds to FLIPs in inner rings and standard elements in outer.

It can be shown by systematic calculations of all other possible loading patterns (e.g. more that two regions, mixed rings), that power defect always lies in the region closed by the two curves in Fig. 16.

Similar results are presented in Fig. 17 for LEU/standard fuel combination.

Both diagrams are derived for average specific power 6 kW/el. (390 kW/66 elements). Diagrams can be used also at lower average specific powers by applying linear interpolation. However, linear extrapolation is not adequate for higher values and gives overpredicted results. Explicit reactor calculations are required. Above results were calculated using TRIGAP code. Reactor power coefficient and

defect can be calculated with fuel management programme like TRIGAP, provided that temperature tabulation is included in the cross section library and that fuel temperature distribution over the core is calculated. Both requirements are met in TRIGAP. The cross sections are tabulated as functions of temperature for several burn-up values and for all fuel types, so that spectral effects on α_f due to burn-up and fuel type are taken into account. WIMS-D/4 is used for preparing the library of TRIGAP as described above.

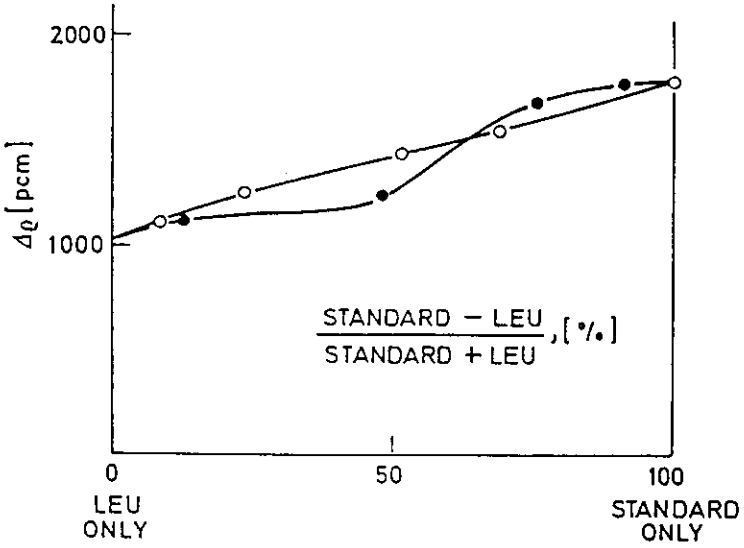

Figure 17: Power defect $\Delta\rho$ of a mixed core with 66 elements as a function of its composition for two loading strategies
circles: standard elements 8.5 w/o U in inner rings, LEU in outer
dots: LEU elements in inner rings, standard 8.5 w/o U in outer

4 Conclusion

Calculations of power peaking factors and α_f using WIMS and TRIGAP code are treated for small TRIGA reactor. Results apply also to other similar cell and reactor geometries and can be used for rough estimates in practical safety analysis. However, in general peaking factors and α_f of a mixed core can not be determined without detailed reactor calculation.

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