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Study on the Quantitative Rod Internal Pressure Design Criterion

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정량적인 핵연료봉 내압 설계기준에 관한 연구

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Abstracts

The current rod internal pressure criterion permits fuel rods to operate with internal pressures in excess of system pressure only if internal overpressure does not cause the diametral gap enlargement. In this study, the generic allowable internal gas pressure not violating this criterion is estimated as a function of rod power. The results show that the generic allowable internal gas pressure decreases linearly with the increase of rod power. Application of the generic allowable internal gas pressure for the rod internal pressure design criterion will result in the simplication of the current design procedure for checking the diametral gap enlargement caused by internal overpressure because according to the current design procedure the cladding creepout rate should be compared with the fuel swelling rate at each axial node at each time step whenever internal pressure exceeds the system pressure.

요 약

핵연료봉 내압 설계기준에 의하면 핵연료봉 갭의 증가만 발생하지 않는다면 봉내압이 계통압력을 초과할 수 있다. 본 연구에서는 이러한 봉내압 설계기준에 따른 정량적이고 보수적인 허용 봉내압을 봉출력의 함수로 생산하였는 바 허용 봉내압은 봉출력의 증가에 따라 감소하였다. 한편, 본 연구에서 구한 허용 봉내압을 봉내압 설계기준으로 적용하면 핵연료봉 압력 검증을 위한 현행 설계절차를 단순화 시킨다. 왜냐하면, 봉내압이 계통압력을 초과할 경우 각각의 주어진 시간 및 축방향 지점에서 핵연료봉 갭 증가를 계산해야 하는 현행 설계절차가 불필요하기 때문이다.

1. Introduction

One of the aims of the fuel rod design is the assurance of the fuel rod integrity under the ANS conditions I and II. In order to ensure the fuel rod integrity, the limiting fuel rod performance with appropriate allowance for uncertainties is shown to be within the limits specified by each fuel rod design criterion. However, it is generally understood that one of the key design criteria with regard to high burnup fuel is the fuel rod internal pressure. The current design criterion for the internal pressure is described as follows : The internal pressure of the limiting rod will be limited to a value below that which could cause the diametral gap to increase [1]. This criterion allows the steady–state rod internal pressure to exceed the system pressure to a certain pressure which depends on the in–pile characteristics of the fuel and cladding, physics data and thermal hydraulic data.

The current procedure for checking the rod internal pressure criterion is to compare the clad creepout rate with the fuel swelling rate computed by the fuel rod design code at each axial node at each time step whenever the internal pressure exceeds the system pressure. However, this procedure is found to be time–consuming and very inconvenient to be employed. In this study, therefore, the generic allowable rod internal pressures not causing the diametral gap enlargement are estimated as a function of rod power with the use of each fuel vendor's clad creepout and fuel swelling rate and they can be used directly as the quantitative rod internal pressure criterion.

2. Description of Model

In order to calculate the generic allowable internal pressure not causing the diametral gap enlargement, the parameters affecting the diametral gap size are to be considered conservatively. The parameters considered are :

- — fuel swelling rate
- — cladding tube creep rate
- — dimensions

2.1 Fuel Swelling Rate

The pellet diameter change is the result of the superposition of a densification and a swelling mechanism. Whereas the first process decreases the pellet diameter by porosity reduction [2] but does not affect the matrix density, the second one increases the pellet diameter by decreasing the matrix density [3]. Those two processes are thought to work simultaneously. Therefore, the overall in–reactor volume change of fuel is given by

$$\frac{\blacktriangle V(B)}{V_O} = -\left.\frac{\blacktriangle V}{V_O}\right|_{dens} + \left.\frac{\blacktriangle V}{V_O}\right|_{swell}$$

where B=Burnup

As shown in Fig. 1 [3], however, a densification mechanism is active only in the low burnup range. Since the internal overpressurization can not occur in the low burup range, only fuel swelling rate is taken into account to estimate the fuel diametral change rate.

According to KWU tests, unrestrained fuel swelling rate is 1 %/(10 MWD/kg(U)) [3] with a scatter of ± 15 % in the burnup range from 12 to 45 MWD/kg(U). This swelling rate represents solid swelling rate since the effect of gaseous swelling is negligible. Fig. 2 shows a comparison of the published data [3] with the swelling rate established by KWU. There is a high degree of accordance and the published data indicates slightly increased swelling rate at high burnups. Therefore, the smallest swelling rate of 0.85 %/(10 MWD/kg(U)) is employed to calculate the fuel diametral strain rate conservatively. Note that the smaller swelling rate generates the lower internal overpressure for the diametral gap enlargement.

Fuel swelling rate proposed by KWU and CE is :

$$\frac{\blacktriangle V_F}{V_{FO}} = V_S\frac{F_V(\blacktriangle t)}{10^{20}/cm^3} \tag{2.1}$$

where $\blacktriangle V_F$=absolute pellet volume increase during the time interval $\blacktriangle t$

V_{FO}=as–fabricated pellet volume

V_S=relative volume expansion due to solid swelling per 10^{20} fissions/cm^3 =0.85%/(10MWD/kg(U))= 0.34% / (10^{20} fissions/cm^3)

$F_V(\blacktriangle t)$=number of fissions/cm^3 during the time interval $\blacktriangle t$

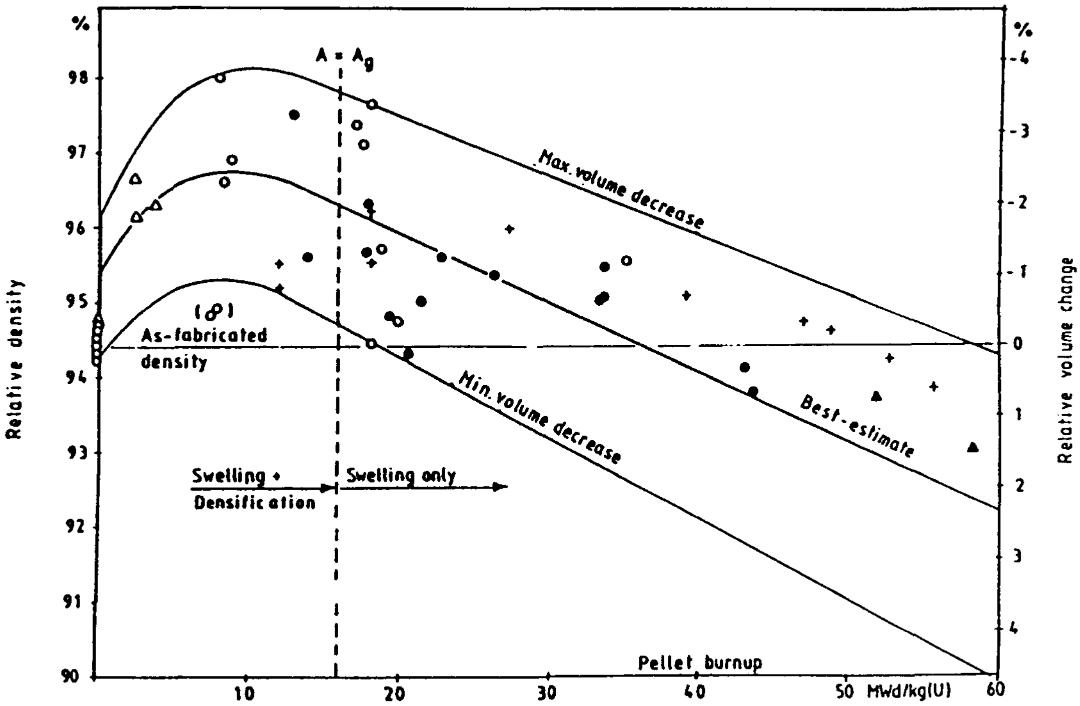

Fig. 1 Pellet Density vs. Burnup(KWU Fuel, Standard Density)

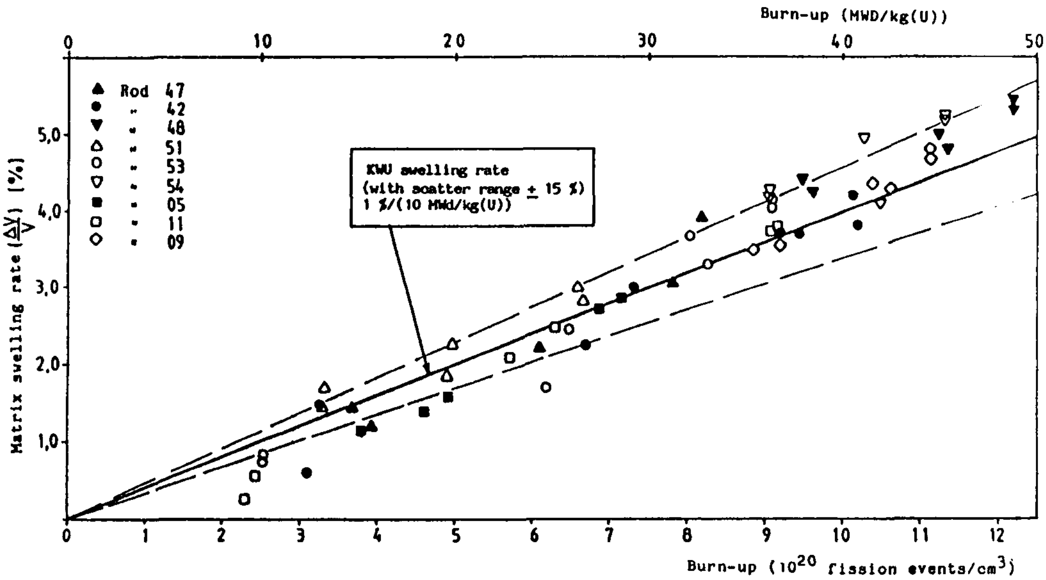

Fig. 2 Comparison of KWU Matrix Swelling Rate with Published Data

$F_V(\blacktriangle t)$ can be written as :

$$F_V(\blacktriangle t)=\frac{F'(\blacktriangle t)\ \blacktriangle t}{V}$$

$$=\frac{q_{th}\ \blacktriangle t}{\pi/4\ D_{FO}^2\ LE}=\frac{q'_{th}\ \blacktriangle t}{\pi/4\ D_{FO}^2\ E}$$

$$F'_V(\blacktriangle t)=\frac{F'(\blacktriangle t)}{V}=\frac{q'_{th}}{\pi/4\ D_{FO}^2\ E} \tag{2.2}$$

where $F'(\blacktriangle t)$ = fission rate during the time interval $\blacktriangle t$

$F'_V(\blacktriangle t)$ = fission rate per unit volume during the time interval $\blacktriangle t$

q_{th} = heat generation rate in the volume V in the time interval $\blacktriangle t$

q_{th} $=q_{th}/L$

E = thermally effective energy per fission $=3.708\times10^{-16}$W day

V = as-fabricated pellet volume $=\pi/4\ D_{FO}^2\ L$

D_{FO} = pellet diameter

L = pellet length

Combining Eq.(2.1) and Eq.(2.2), pellet diametral strain rate may be represented as follows, assuming the pellet diametral change to be one—third of the pellet volume change :

$$\frac{\blacktriangle\dot{D}_F}{D_{CO}}=1/3\frac{\blacktriangle\dot{V}_F}{V_{FO}}\frac{D_{FO}}{D_{CO}}$$

$$=1/3\frac{V_S\ q'_{th}\times10^{-20}}{\pi/4\ D_{FO}\ E\ D_{CO}}$$

$$=\frac{3.9\times10^{-8}\ q'_{th}}{D_{FO}\ D_{CO}} \tag{2.3}$$

where D_{CD} = cladding outside diameter

Note that the reference dimension for strain is the cladding outside diameter. From Eq. (2.3), it can be seen that the pellet diametral strain depends on a rod power, the pellet and clad diameters.

2.2 Cladding Tube Creep Rate.

According to Ref. [4], the true creep rate under reactor conditions is composed of a thermally activated and a radiation-induced contribution. In addition, the cladding tube creep is usually divided into primary and secondary creep. The thermal creep and the irradiation-induced creep strains depend on the cladding manufacturing process. KWU [5] recommends the following creep formula for the cladding tube manufactured by the KWU specification.

$$\in_{tot}=\in_{1,th}+\in_{2,th}+\in_{1,irr}+\in_{2,irr}$$

where $\in_{tot}$ = total hoop creep strain

$\in_{1,th}$ = primary thermal hoop creep strain

$\in_{2,th}$ = secondary thermal hoop creep strain

$\in_{1,irr}$ = primary irradiation-induced hoop creep strain

$\in_{2,irr}$ = secondary irradiation—induced hoop creep strain

$$\in_{tot}=\in_{1,th}+\in_{2,th}+\in_{1,irr}+\in_{2,irr}$$

$$=\{c1\ [1-\exp(-k\sqrt{t})]+t\}\ (\in_{th}+\in_{irr}) \tag{2.4}$$

where $\in_{th}=b\ \exp(-\theta_2/T)\ \sigma_{eq}^{1.87}\ \tau/|\tau|$

$\in_{irr}=c\ \phi^{0.85}\ \tau/|\tau|$

k $=a\ \exp(-\theta_1/T)$

a, b, c, c1, θ_1, θ_2 = constants

ϕ = fast neutron flux(10^{13}n/cm^2–sec)

T = cladding average temperature (°K)

$\sigma_{eq}=\sqrt{3}\ |\tau|$

τ $=\sigma_{tg}-\sigma_{ax}$

t = elapsed time after interval gas pressure exceeds the system pressure

$$\sigma_{tg}=\frac{P_G D_{ci}-P_w D_{ma}}{D_{ma}-D_{ci}}$$

$$\sigma_{ax}=\frac{P_G D_{ci}^2-P_w D_{ma}^2}{D_{ma}^2-D_{ci}^2}$$

P_G = internal gas pressure

P_W = system pressure

D_{ci} = inner diameter of cladding

D_{ma} = outer diameter of metallic cladding

Differentiating $\in_{tot}$ with respect to t gives $\dot{\in}_{tot}$ as :

$$\dot{\in}_{tot} = [\frac{C1\ k}{2\sqrt{t}} \exp(-k\sqrt{t}) + 1]\ (\in_{th} + \in_{irr}) \qquad (2.5)$$

CE [6] recommends the following creep rate formula for the tubes manufactured by the CE specification :

$$\dot{\in}_{tot} = \dot{\in}_{2,th} + \dot{\in}_{1,irr} + \dot{\in}_{2,irr}$$

$$\dot{\in}_{tot} = A[\sinh(B\ \sigma_{eq})\ \exp[(-C+D\ \sigma_{tg})/RT] + [E\exp(-Ft)+G]\ \phi^{0.85}\sigma_{tg}^{1.5} \exp(-\theta_3/RT) \qquad (2.6)$$

where A, B, C, D, E, F, G, θ_3 = constants

In this study, the maximum cladding creep model constants proposed by KWU and CE are taken into account to estimate the maximum cladding creepout at the same power.

2.3 Dimensions

Pellet and clad dimensions can be varied within the tolerance limits. In the fuel rod design codes, usually the upper tolerance limit of clad and the lower tolerance limit of pellet are utilized in order to consider larger fission gas release caused by high fuel temperature. The cladding creepout rate will be accelerated with an increase of the cladding temperature which depends strongly on the axial position and the oxide layer thickness. It is found that the maximum cladding temperature occurs at about three quarters of active fuel length. In order to estimate the conservative creepout rate, therefore, it is assumed that the maximum oxide layer thickness of 100 μm [1] allowed by the oxide thickness criterion exists at the outer surface of the cladding the axial position of which lies at the three quarters of the active fuel length. Note that oxide layer thickness depends strongly on power hisory and burnup. Since the maximum allowable oxide layer thickness of 100μm is employed for the calculation of the cladding temperature, the cladding temperature used in this study covers all power histories and burnups. Also the conservative thermal hydraulic data with respect to cladding temperature are employed. Table 1 summarizes the pellet and clad dimensions, the clad average temperatures and the fast neutron fluxes which are relevant to the Korean nuclear power plants. The calculation procedure for the allowable rod internal pressures

Table 1. Dimensions and Cladding Average Temperatures vs. FA Type

FA Type	Dimensions			Clad Average Temp.(℃)/Fast Neutron Flux(x10^{13}n/cm^2 S)							
	Pellet	Clad	Clad*	Linear Heat Generation Rate(W/cm)							
	O.D(mm)	I.D.(mm)	O.D.(mm)	160	180	200	210	220	230	240	250
14×14 (Kori-1)	9.100	9.230	10.670			392 10.45		400 11.50	404 12.02	408 12.54	412 13.07
16×16 (Yonggwang 3&4)	8.255	8.433	9.573	386 11.14	395 12.55	403 13.94	408 14.63	412 15.23			
17×17 (Yonggwang 1&2)	8.040	8.260	9.420	391 10.80	400 12.15	408 13.50	413 14.18	417 14.85			

* Clad O.D. represents the outer diameter of metallic cladding with 100 μm oxide layer thickness at the outer surface.

** Max. clad. average temperature is obtained considering the conservative thermal hydraulic data of each fuel assembly type, the axial position of the cladding, and the maximum allowable oxide layer thickness of 100 μm.

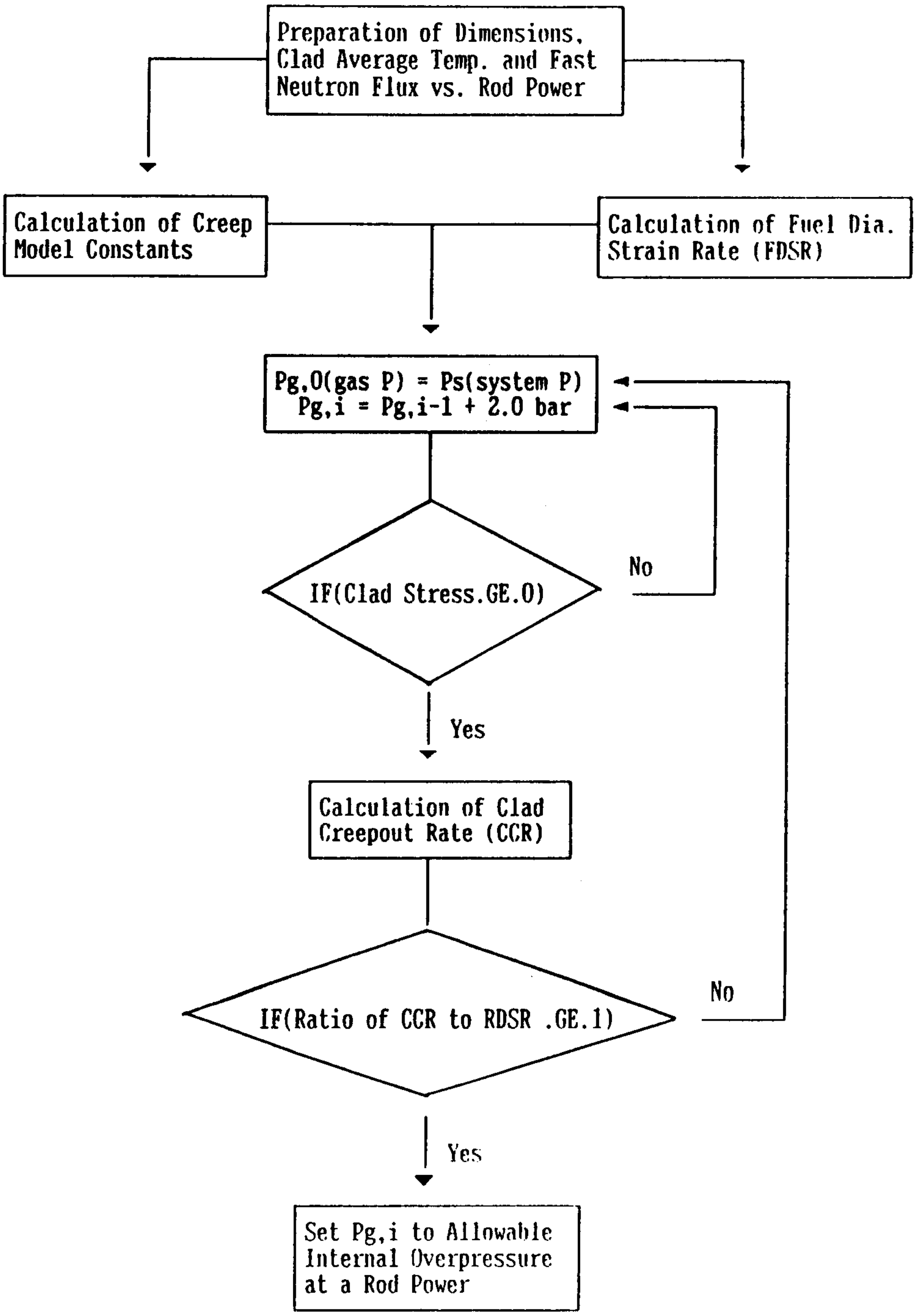

Fig. 3 Schematic Diagram for the Allowable Allowable Internal Overpressure Calculation

not causing the diametral gap enlargement is given in Fig. 3.

3. Results and Discussion

The allowable rod internal overpressure not causing the diametral gap enlargement is calculated as a function of a rod power. The ratios of cladding creepout to fuel swelling rate are plotted in Figs. 4 and 5 for Kori-2(16×16 FA, KWU cladding tube) and Yonggwang-3&4(16×16 FA, CE cladding tube), respectively. These figures show that the gradient of the curve becomes larger with an increase of a rod power and/or inter-

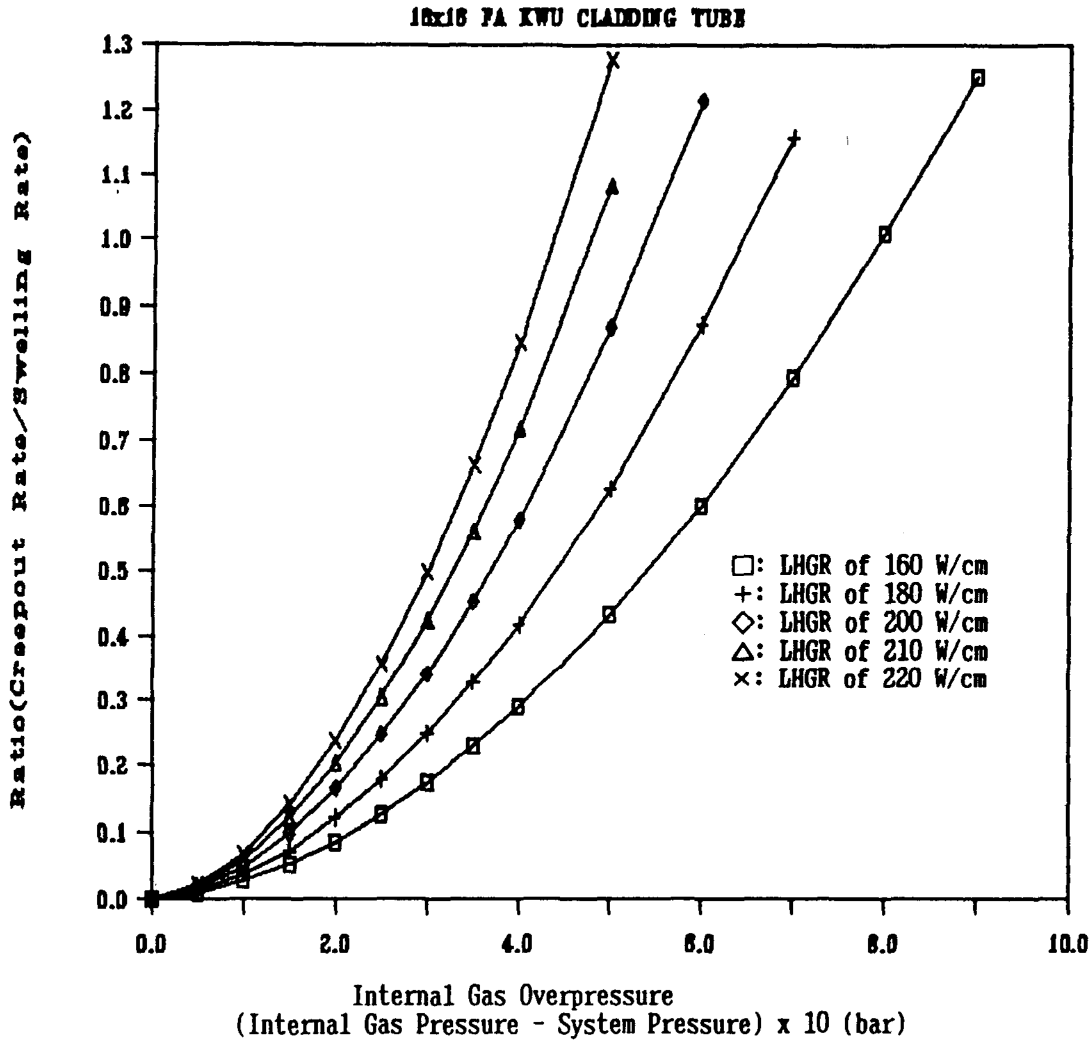

Fig. 4 Ratio of the Cladding Creepout Rate to the Fuel Swelling Rate versus Internal Gas Overpressure [Kori-2 (16×16 FA)-KWU Cladding Tube]

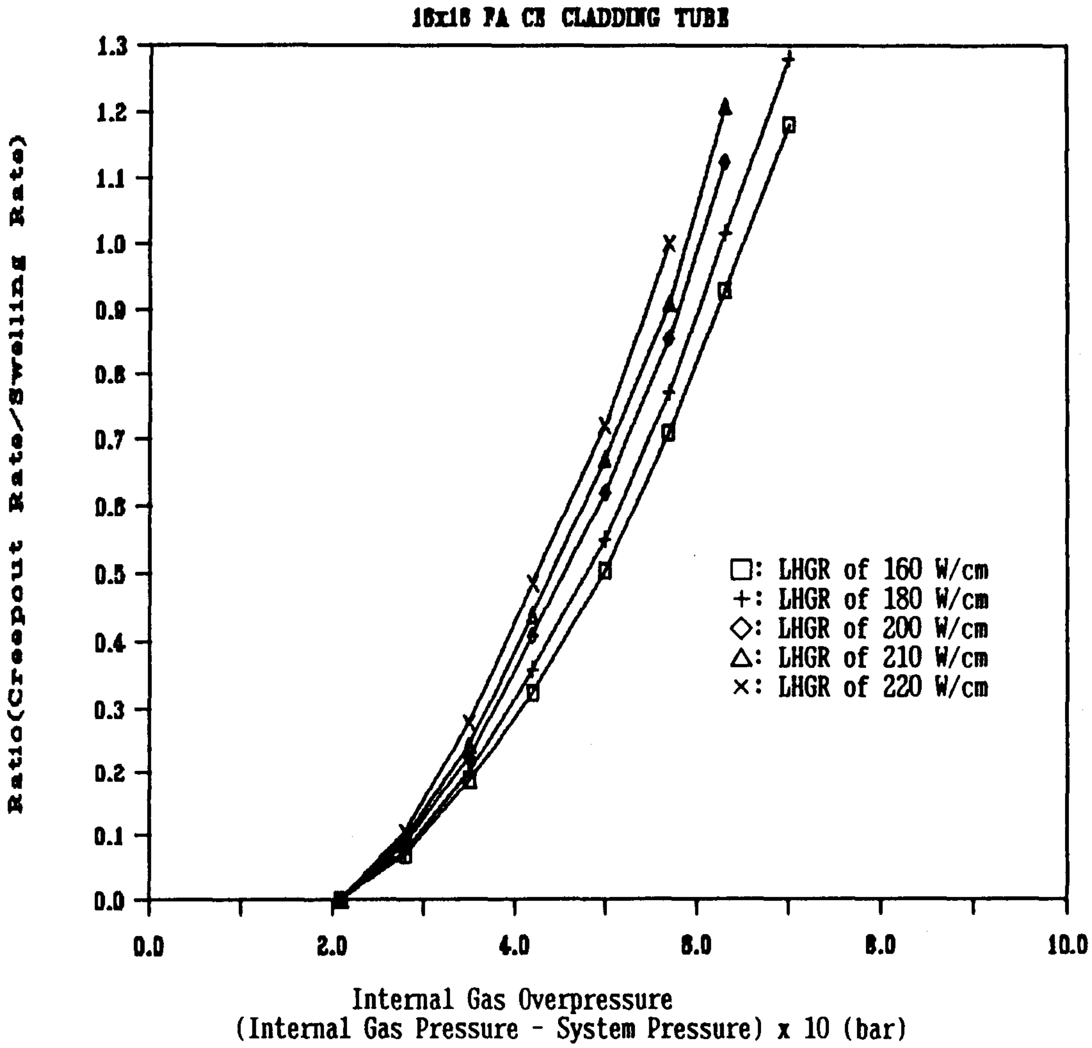

Fig. 5 Ratio of the Cladding Creepout Rate to the Fuel Swelling Rate versus Internal Gas Overpressure [Yonggwang–3&4(16×16 FA)–CE Cladding Tube]

nal overpressure. In addition, the cladding creepout for the KWU cladding tube occurs when internal gas pressure exceeds system pressure, as shown in Fig. 4, while the cladding creepout for the CE cladding tube appears to occur when internal pressure is approximately 13 % greater than the system pressure,as shown in Fig.5. This can be explained by the cladding stress used in the creep formulas: The maximum shear stress($\tau = \sigma_{tg} - \sigma_{ax}$) is employed in the KWU creep formula(see Eq.(2.4)) while the tangential stress (σ_{tg}) in the CE creep formula (see Eq.(2.6)). Considering that the ratio of greater than or equal to 1 means the diametral gap enlargement appearance, the internal gas overpressures at the ratio of 1 are plotted for each fuel assembly in Fig. 6. From this figure, it can be seen that the allowable internal overpressure is approximated to decrease linearly

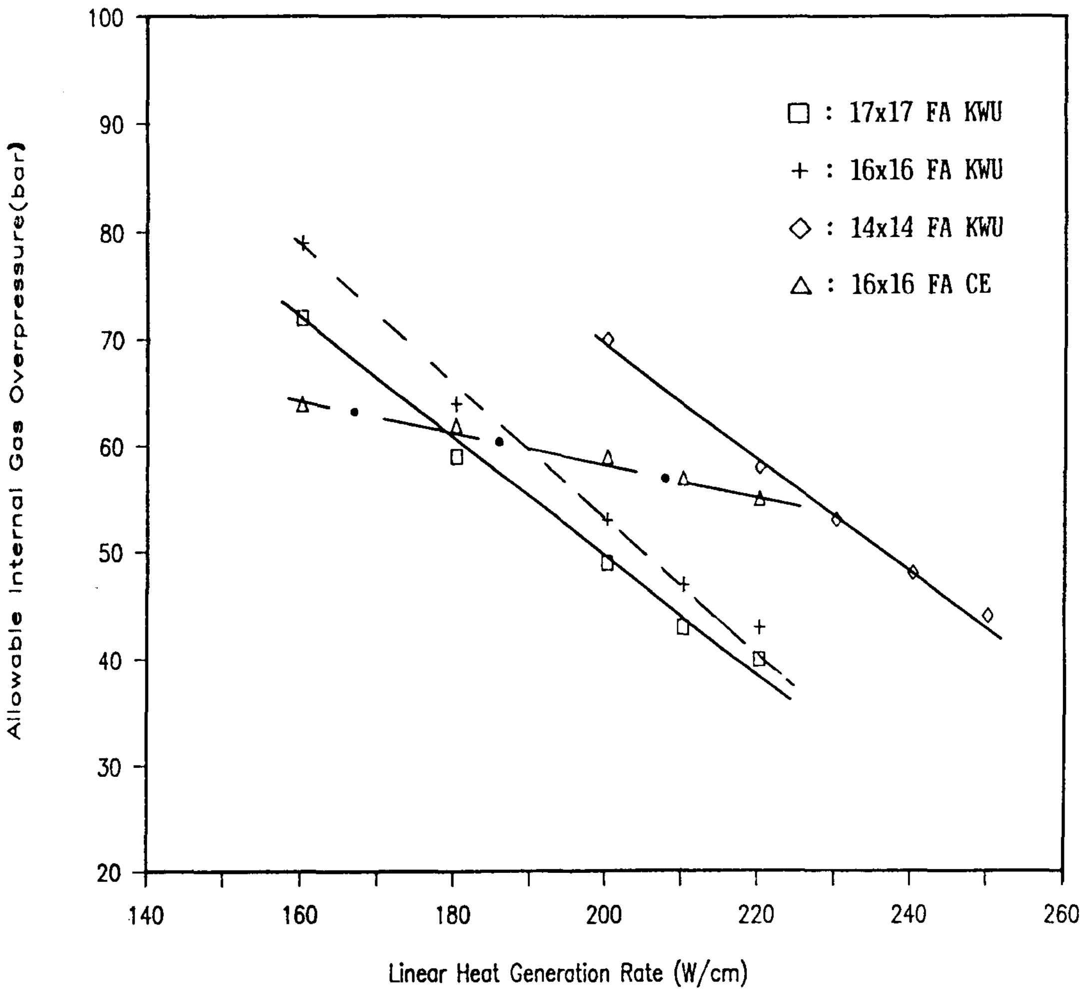

Fig. 6 The Generic Allowable Internal Gas Over pressure for Each Fuel Assembly versus Local LinearHeat Generation Rate

with an increase of a rod power. The relationships for each fuel assembly are represented as follows :

▲P (bar)=−0.540 q′ (W/cm)+177.0
for Kori-1 (14×14 FA)

▲P (bar)=−0.583 q′ (W/cm)+170.3
for Kori-2 (16×16 FA)

▲P (bar)=−0.517 q′ (W/cm)+153.7
for Yonggwang-1&2 (17×17 FA)

▲P (bar)=−0.142 q′ (W/cm)+87.2
for Yonggwang-3&4 (16×16 FA)

where ▲P=allowable internal gas overpressure
q′=local linear heat generation rate

Comparison of the above relationships shows that the slope for Yonggwang-3&4 (CE cladding tube) is much different since the thermal creep activation energy used in the CE thermal creep formula is much smaller than that used in the KWU one, as given in Eqs.(2.4) and (2.6). In addition, the allowable gas overpressures for Yonggwang-3&4 are larger in the relatively high power range than those for Kori-2 since the former uses the tangential stress and the lower thermal activation energy, as explained above. For the KWU cladding tube, on the other hand, 14×14 FA, 16×16 FA and 17×17 FA are ordered in the magnitude of gas pressure. This is because the higher cladding temperature and the larger fast neutron flux give the larger creepout rate at the same power. Table 1 shows that 14×14 FA and 17×17 FA have the lowest and the highest temperatures, respectively, at the same rod power. In addition, 14×14 FA has the smallest fast neutron flux, and 16×16 FA and 17×17 FA have about the same fast neutron flux.

For the fuel rods loaded in Yonggwang-2, the diametral gap variations at each axial node at each time step are calculated as a function of a rod power with an aid of the fuel rod design code to check the applicability of the allowable internal overpressure determined in this study for the quantitative rod internal pressure criterion. In Fig. 7, the allowable internal overpressures are compared with the minimum internal overpressures which are obtained by examining the diametral gap size variation at each axial node at each time step. This figure shows that the allowable

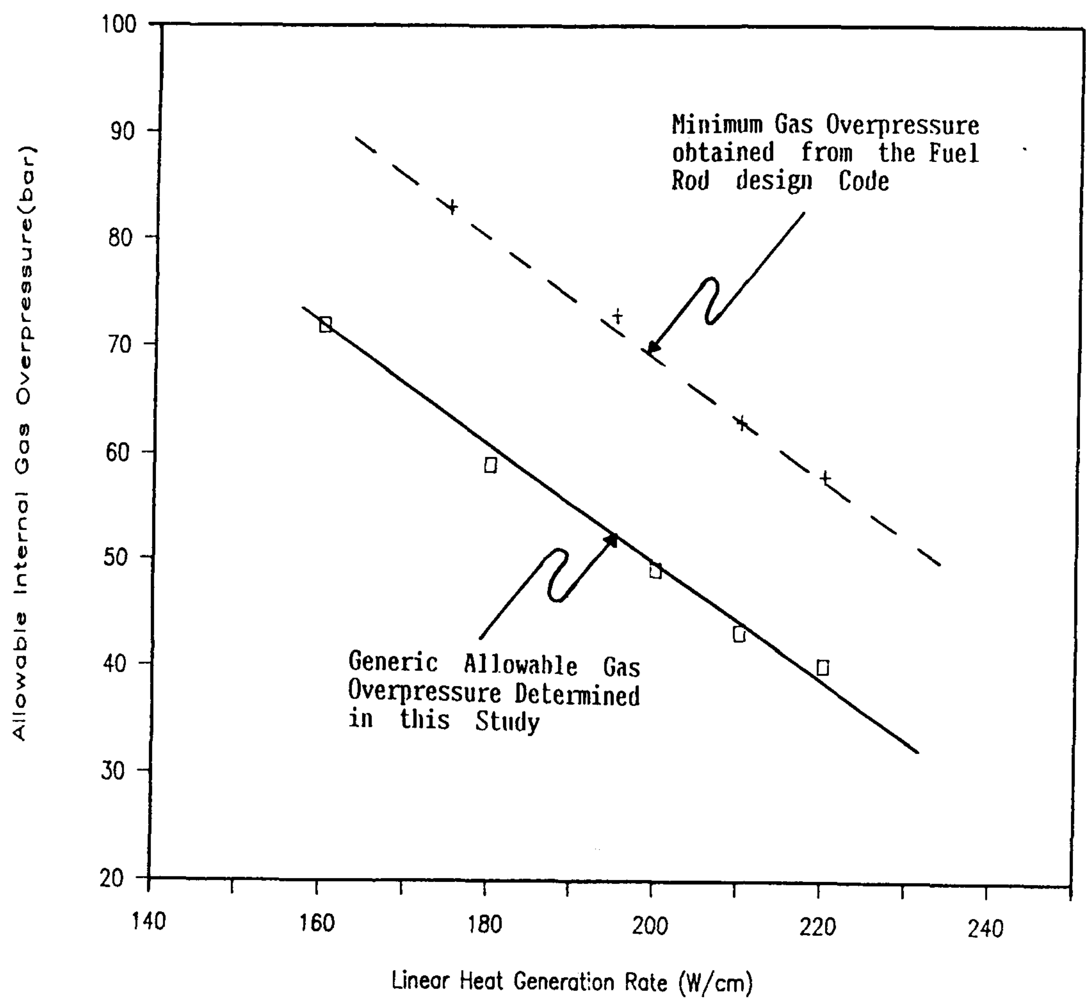

Fig. 7 Comparison of the Generic Allowable Internal Gas Overpressure and the Minimum Internal Gas Overpressure obtained from the Fuel Rod Design Code

internal overpressures are approximately 15 bar smaller than the minimum internal overpressures in the rod power range considered. This difference can be explained by comparing the cladding creepout rate and the cladding temperature. In the fuel rod design code, the same creep model constants are to be employed regardless of the condition of cladding creepdown or cpeepout due to the design code calculation structure. The design code employs the minimum cladding creep for the rod internal pressure calculation in order to generate the largest diametral gap which causes the maximum fission gas release during the cladding creepdown condition. However, the maximum cladding creep and the higher cladding temperature which are employed for the allowable internal gas pressures determined in this study will accelerate the diametral gap enlargement appearance during the cladding creepout condition. Therefore, the allowable overpressure is lower than the minimum overpressure calculated with the use of the minimum creep and the former can be employed as the quantitative rod internal pressure design criterion which is to be compared with the rod internal pressure calculated by the fuel rod design code.

4. Summary

In this study, the generic allowable internal gas pressures not violating the diametral gap enlargement are determined as a function of a rod power with the use of the conservative inpile fuel and clad performance characteristics, the physics and thermal hydraulic data which are relevant to the Korean nuclear plants. The results indicate that the allowable internal overpressure can be approximated to decrease linearly with an increase of a rod power as follows :

$$\blacktriangle P \text{ (bar)} = -0.540\ q' \text{ (W/cm)} + 177.0 \quad \text{for Kori-1 } (14\times14 \text{ FA})$$

$$\blacktriangle P \text{ (bar)} = -0.583\ q' \text{ (W/cm)} + 170.3 \quad \text{for Kori-2 } (16\times16 \text{ FA})$$

$$\blacktriangle P \text{ (bar)} = -0.517\ q' \text{ (W/cm)} + 153.7 \quad \text{for Yonggwang-1\&2 } (17\times17 \text{ FA})$$

$$\blacktriangle P \text{ (bar)} = -0.142\ q' \text{ (W/cm)} + 87.2 \quad \text{for Yonggwang-3\&4 } (16\times16 \text{ FA})$$

The relationships for the allowable internal gas overpressure can be employed as the quantitative internal pressure design criterion with reasonably sufficient conservatism. The application of this quantitative rod internal pressure criterion will eliminate a time-consuming procedure for checking the cladding creepout rate and the fuel swelling rate at each axial node at each time step whenever internal pressure exceeds system pressure.

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