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Fast Reactor Core Heat Removal

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x) Work performed within the association in the field of fast reactors between the European Atomic Energy Community and Gesellschaft für Kernforschung mbH.,Karlsruhe

FAST REACTOR CORE HEAT REMOVAL

1. Introduction

The question of heat removal is identical with the question of the best method for fast reactor cooling, but different from the question for the best fast reactor coolant. The latter question is much broader and includes not only thermohydraulic qualities, but also physics characteristics and the implications of the whole primary circuit. This paper concentrates mainly on problems of the first kind, namely core cooling, but occasionally we cannot do without considering the general coolant discussion too. I shall try to explain the thermal problems without cutting off other considerations.

Our problem is stated as follows:
Given a parallel array of metal clad rods filled with PuO_2-UO_2 of 80 - 85 % of theoretical density, with an axial blanket part at either end and a fission gas plenum at one end -, and given maximum permitted temperatures for fuel and cladding: Arrange and cool this in a way, that none of the maximum temperatures are exceeded and that the power generating costs will have a minimum. Naturally a number of boundary conditions has to be fulfilled of whom I only mention safety and operational reliability. I shall touch them occasionally.

The dependence of the power generating costs on core design is schematically shown in fig. 1. The normal expressions for power cost primarily depend on the parameters listed in the second column. We neglect the dependence on nontechnical parameters like interest rates, load factors, Pu-price, but also unit costs for fabrication, transport, reprocessing etc. The primary parameters themselves are dependent on quite a number of core design parameters as listed in column 3. We cannot explore the details of these interdependences in a paper like this, but we shall draw some lines of practical understanding. We shall call these core parameters "internal " parameters.

Also to an even larger extent the power costs depend on the design and components of the coolant circuit and the total plant outlay as shown in fig. 2. We shall define these parameters as "external" parameters.

Mainly three coolants are being discussed for fast reactors: Sodium, steam and Helium (or CO_2). Sodium has by far the best cooling potential and turns out to be very favorable with respect to the internal parameters. On the other hand, it has not been demonstrated yet, that the external parameters, especially the steam generator, will favour a low cost solution.

Steam is just the opposite: The internal parameters are unfavourable resulting in low breeding, low burnup, moderate efficiencies and many problems with corrosion, safety, tolerances etc. On the other side it is expected that the external parameters, based on light water reactor technology will allow for low cost solutions. This too has not been demonstrated so far and it might take a long time since in the USA the steam activities have been reduced and also in Germany they are under reconsideration.

Gas offers some advantages and some disadvantages both for the internal and external parameters. It probably has a very good future potential in the long range and may profit from the experience with HTGR's. At present all gas work is at a very early stage, at this looking very prosperous, but many details have to be cleared before it can be evaluated as sodium today.

We shall discuss all three coolants in the following sections. Before doing so, we already can fix two parameters of the third column of fig. 1: The rod power q_r and the rod diameter d_r.

For fast reactors the relation between q_r and the conductivity integral

(1) $$q_r = 4\pi \int_{T_1}^{T_o} k \, dT \quad [\mathrm{W/cm}]$$

T_o = fuel center temperature
T_1 = fuel surface temperature
k = thermal conductivity of fuel

is quite exact. The economic optimum for sodium cooling is the maximum rod power, where T_o comes close to the fuel melting temperature [5]. It has been shown, that this is also true for steam cooling [2,3], so that (neglecting the weak dependence on T_1) $q_r \approx 500$ W/cm at the hot spot, the average value of q_r is then given by consideration of hot channel factors and power distribution. For sodium and also gas cooling carbide fuel has been proposed and offers some advantages.
But here rod powers of 1000 - 1500 W/cm [5] are required for economical and technical reasons.

The rod diameter d_r in all designs turns out to be 5 - 6 mm, as a result of fuel cycle cost optimization betweeen fabrication costs and plutonium interest rates, quite legally neglecting other dependences [5,6]. Since this is about half the value of PWR's, the heat flux has at least double the value, amounting to 200 - 300 W/cm^2. For carbide fuel rods larger rod diameters are more economic [4] and technically feasible because of the large heat fluxes at the surface.

2. Sodium

2.1 Temperature Limits

The maximum fuel temperature already has been considered in connection with the rod power in the preceding paragraph. The thermal conductivity k depends on fuel density, and this is fixed by swelling with burnup. Karsten [7] has combined the existing knowledge into a simple swelling model where he assumes a "plastic" fuel zone ($T > 1700^\circ$C), a "creep" zone ($T = 1300 - 1700^\circ$C) and a "cold" zone ($T < 1300^\circ$C). To avoid an extensive swelling pressure on the cladding the fuel density for burnups up to 80 000 - 100 000 MWd/t has to be in the range of 80 - 82 % of the theoretical value. (Compared to earlier designs with 90 % th.d. this has resulted in a reduction of breeding ratios by 0.05 with any coolant).

The maximum clad temperature is determined by mechanical criteria. The maximum material stress occurs at the end of the fuel lifetime and is given by the combination of fission gas pressure, fuel swelling pressure, and thermal stress (fig. 3). This may lead to pin failure by one of the following mechanisms:

a) Exceeding the yield strength in the interesting temperature range in question of 650 to 750°C will result in clad failure /‾9_7.

b) Strain cycling may result in fatigue failure.

c) For irradiated material the rupture strain is in the range of 1 °/o (high temperature embrittlement). Therefore creep deformation up to this value must be avoided /‾8_7. For most designs this determines the maximum allowable cladding temperature.

But there is still another mechanism:

d) The combination of creep and strain cycling results in a certain type of thermal ratcheting. This again leads to failure above a certain strain, limited by irradiation effects. Compared to the continuous creep mentioned under c) this effect is the more dangerous one. While in the continuous case thermal stresses are degraded, they reappear partially in the cyclic case. Therefore this effect normally defines the limiting value for the fuel element design and the limiting temperatures /‾10_7.

Thus in general by one or the other of these criteria there is given a certain maximum fission gas and fuel swelling pressure. Since the fission gas pressure also depends on the length of the fission gas plenum, there is no unique relation between for example burnup and clad temperature. Rather the allowable can temperature is a function of

burnup (amount of fission gas release and fuel swelling),
clad material,
wall thickness (for physics reasons as low as possible),
length of plenum h_p,
number of power cycles during fuel lifetime,
heat flux in cladding.

Fig. 4 shows the permittable fission gas pressure for the cladding X8 Cr Ni Mo V Nb 1613 as a function of wall thickness at 700°C as limited by the thermal ratcheting criterium according to a theory developed by G.Schmidt /‾11_7 (criterion d) and according to criteria a) and c).

The following assumptions have been made:

$T_{clad} = 696\ ^{o}C$
number of cycles = 144
swelling pressure accord.to Karsten/⁻7_7 $\rho_f = 80\ ^{o}/o$ th. d.

However, the creep rate under irradiation is not well known. Therefore the dotted line shows the allowable pressure under the assumption of a creep velocity 10 times larger than the unirradiated value. For the unirradiated material this corresponds to a 35 ^{o}C rise in temperature.

Fig. 5 /⁻10_7 gives the length of the fission gas plenum for the same fuel rod as a function of wall thickness for a burnup of 68 000 MWd/t (axial average) if 50 $^{o}/o$ or 100$^{o}/o$ of the produced fission gas is liberated from the fuel, and under the assumption of normal and tenfold creep rates. Even under this wide range of input variables one stays within a reasonable range of plenum lengths between 5 and 90 cm.

In conclusion: For an austenitic steel cladding a maximum temperature around 700^{o}C is reasonable, but a range of $\pm$ 20^{o}C is also not forbidden by principal considerations. The temperature optimization is not possible yet and has to include the variables burnup, wall thickness (breeding ratio and critical mass) and plenum length (pressure drop). Other materials, like Vanadium alloys /⁻14_7 are being contemplated, but no quantitative argument can be given today.

2.2 Normal Cooling Conditions

In order to reach this cladding temperature quite a number of cooling parameters must be established, of which the actual heat transfer coefficient is of minor importance. This can be seen from fig. 6 /⁻10_7, where for a number of cases the inlet temperature ϑ_1 and the inlet-outlet temperature difference $\Delta\vartheta$ has been varied. The maximum inside cladding temperature (top line) is given by the several parameters as follows:

a) The hottest point of the cladding for sodium cooling is practically always at the core outlet. Therefore the total $\Delta\vartheta$ must be taken into account.

b) The radial blanket in this case is cooled in parallel flow to the core. At the end of the blanket life it produces about 10 °/o of the thermal power. If the blanket is fresh, this additional power has to be produced by the core and raises its temperature.

c) In the peripheral channels of each subassembly the sodium flow normally is different from that in the inner channels for geometrical reasons. For a given average outlet temperature temperatures in local channels therefore will differ from the average.

d) Depending on the number of refueling cycles per fuel lifetime there is a certain burnup swing. For our example the cycle number was 3.

e) Because of the small amount of mixing in a subassembly, for the case of a radial power gradient the inner side subchannels are at larger power than those on the peripheral side. This results in temperature gradients especially with the low mixing ability of the grid spacers as used in all sodium cooled core designs for reasons of axial rod movement. Optimum orificing of the coolant flow will provide for the same maximum outlet temperatures.

f) The hot channel factors are of particular importance. Table 1 shows the values used for this example [10]. Here a semistatistical approach has been used, distinguishing between statistical and systematical errors. Besides this the connection between confidence level and error distribution and their effect on parallel flow channels must be considered [15]. A study [16] shows, that a complete statistical approach for a confidence level of 97,7 °/o leads to a similar overall hot channel factor as in table 1. Since the hot channel temperature rise is proportional to the nominal temperature rise, the largest cladding temperatures are obtained for the largest $\Delta\vartheta$ as can be seen by comparing case 8 to case 7, where a decrease in inlet temperature and a smaller decrease in outlet temperature still may result in an increase of cladding temperature.

g) The temperature difference coolant to cladding is comparatively small. Normally it may be calculated according to Dwyer [17,43]; some references show this to be too optimistic [12,13,18], but this is of really minor influence.

h) The temperature difference across the cladding finally defines the working temperature with respect to stresses.

Hence, we may formulate the following relation for the max. clad temperature as a function of burnup Bu and plenum length h_p (neglecting other parameters).

(2) $$T_{max}\ (Bu, h_p) \quad = \quad \vartheta_1 + \Delta\vartheta \cdot H + \text{const.}$$

or

(3) $$Bu \quad = \quad Bu\ (h_p, \vartheta_1, \Delta\vartheta \cdot H)$$

where $\Delta\vartheta \cdot H$ is the coolant temperature rise of the hot channel.
(H: hot channel factor)
There is no explicit formulation of eq. (3) available yet (except for the model for ratcheting), and any optimization has to consider the coupling to the whole system of the variables of fig. 1. In fig. 7 the range of different designs with respect to ϑ_1 and $\Delta\vartheta$ is shown /‾10,19,20,21_7.

According to our own work /‾5_7 the optimum $\Delta\vartheta$ should be around 180 °C, but most designs have decided for lower values.
By this the first variable of the first column of fig. 1 is defined.

We now turn to the next variables of the second column of fig. 1.

The rating r is given by

(4) $$r \quad = \quad \frac{\bar{q}_r}{\pi\, d_r\, \rho_f\, e}$$

It is the mentioned optimum between high rating (small d_r) and low fabrication costs (large d_r), which fixes this value. All other variables are of minor importance.

$$r \quad = \quad 0{,}7 - 1{,}0 \text{ MW/kg fissile} \qquad \text{(oxide fuel)}$$

The breeding ratio BR is fixed by physics.

(5) $$BR \quad = \quad BR\ (F_c, h_c, d_c, \rho_f)$$

Low leakage cores are characterized by $h_c \approx d_c$, a large positive void and a large negative Doppler effect and a large internal breeding ratio. Flattening will shift the system to external breeding without changing the overall BR too much. External breeding means large reactivity swings and capital charges on blanket Plutonium. The better void effect is paid for by a worse Doppler coefficient. The most important economical influence of flattening is the increased fabrication cost by the increased number of fuel pins.

A correct optimization of the BR and in connection with that of h_c and d_c has not been made yet. Current designs [10,19,20] favour $h_c \approx$ 80 - 100 cm, which in combination with a 45 - 50 °/o coolant fraction leads to reasonable pressure drops in the order of 2,5 - 5,0 bars. For 1000 MWe this means $h_c/d_c \approx 1/3$. Even in the U.S.A. where flat or otherwise leaky cores have been favoured for some time [23], the same tendency has developed.

Finally the efficiency η of sodium cooled reactors can be expressed in a simple way. As shown in fig. 6 the sodium outlet temperature ϑ_2 has to be about 120 °C below the maximum can temperature. Assuming two sodium systems η can be calculated to the first order as a function of the sodium temperature and, therefore, also as a function of cladding temperature as shown in fig. 7. Efficiencies of 40 - 42 °/o reflect the current status. This is clearly shown on <u>fig. 8</u> for different designs.

Concluding this paragraph on normal core cooling with sodium, the main areas for further improvement are as follows:

a) Better power flattening to get larger average rod powers.

b) Better coolant mixing in subassemblies allows for larger efficiencies at the same can temperature.

c) Better understanding of the cost of tight tolerances versus gain in hot channel factors and by this also give larger efficiencies.

d) Optimization of the h_c/d_c - ratio.

e) Finally as a clearly evident statement: Development of a fuel element with

high burnup - high density fuel - low absorption - low creep - high temperature cladding.

2.3 Sodium Boiling

The normal operation temperatures are 350 °C below the boiling point of sodium. Only under accidental conditions boiling may start and lead to dangerous consequences in connection with the positive void coefficient. We have to distinguish two cases:

a) Boiling over the whole Core Region

This could be caused by a loss of coolant flow combined with a complete safety system failure and is therefore very improbable.
In this case the axial liquid ejection rate and the radial spreading of the boiling zone determine the reactivity input rate and from this the destructive energy of the following excursion. Reactivity rates in the order of 50 $/sec and mechanical energies in the order of 1000 MWsec have been calculated by the various groups. Most groups do not consider this accident to be beyond the design basis accident.

b) Boiling in a Single Subchannel or Subassembly

This could be caused simply by a local flow blockage, fuel element swelling or can failure and is much more probable than a).
In this case the reactivity input is negligible and the ejection rate therefore unimportant. But pressure pulses by the sudden flashing of superheated liquid or even more by recondensation of vapour bubbles may effect the neighbouring channels or subassemblies and result in a fast propagation *) of the failure. Finally then again a fast reactivity input is created and the consequences are similar to case a).

I am not going to discuss the safety aspects of this which mainly depend on the reliability of several engineered safeguards. Rather I shall restrict myself on the aspects of sodium boiling itself.

In connection with the cases a) and b) there are to consider

1) ejection rate,
2) liquid superheat and
3) recondensation.

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*) This is to be distinguished from the slow failure propagation by fuel element melting like in the Enrico Fermi incident.

Ejection Rate

Quite a number of models have been proposed [56,57,58,59,61]. Today the best fit to measurements [49,60] is given by the BLOW-code [50,54]. In a first phase a small bubble forms in the superheated liquid. It is important that in sodium because of superheat normally just one single bubble develops at a time. The second phase describes the growth of a spherical bubble, followed by a third phase with the growth of a now cylindrical bubble, fed by evaporation of a thin liquid film on the heated channel surface. It is particularly this process, which distinguishes the BLOW-model from the earlier ones. In the final phase the liquid film dries out, but this may be overrun by a return flow of the liquid into the channel.
The model still needs some more refinement for the first instants of nucleation and bubble growth, but in general the ejection process does not present any more principal theoretical difficulties.

Liquid Superheat

Liquid superheat is an important input parameter for ejection codes. It is not very well understood today. The overpressure in a bubble of radius r is according to the following wellknown relation:

$$\Delta p = \frac{2\sigma}{r} \qquad (\sigma\text{: surface tension})$$

This corresponds to a certain rise in the saturation temperature. For the initiation of boiling, therefore, a nucleus is needed, which is either in the liquid or at the surface. The nature of these nuclei is still unknown.

- Active cavities at the surface should be destroyed by the wetting action and chemical aggressivity of the long time operation in the liquid phase [54]. Holtz [53] has formulated a phenomenological model on the activity of cavities, but the physico-chemical nature of the activation and formation of the nuclei is by no means clear. Hoffman et al. [51,52] have already used deep cavities for nucleation and in experiments of Schultheiss [65] very simple cavities stayed active even when completely filled with sodium.

- Nucleation by irradiation, according to Claxton [62] must also be excluded.

- Spontaneous (statistical) nucleation in the range of interest is very improbable.

- The effect of dissolved gases is too small [63].

- Only entrained gas bubbles as possible in designs with free surface pumps may have a considerable effect. They constitute the most efficient safety measure against superheat. Effective bubble nuclei still are so small that reactivity disturbances can be avoided.

At the other hand, the measurements, although widely spread, show relatively low superheats under reactor conditions. Values in the order of 25 - 50 ^{o}C are to be expected. So from the practical point of view superheat could be handled, whereas the theoretical insight into the nucleation phenomena still is rather poor. More effort on experiments with controlled physico-chemical conditions of liquid and surfaces is needed.

Recondensation

The reactor system implies heated core channels, unheated axial blanket channels and the sodium pool above and below the core. Vapour bubbles from the core region very soon will reach cooler zones and recondense. Moreover measurements show a periodic flow reversal with sodium returning into the heated region. Basically this is a typical feature of the unstable boiling of the liquid metals [51,52]. Peppler [64] demonstrated this for channels of 50 cm heated length, unheated portions above and below and wall heat fluxes of several hundred W/cm^2 - i.e. typical reactor conditions.

The condensation of bubbles is accompanied by water-hammer type pressure pulses [63], very narrow ($\approx$1 ms) peaks with up to 30 atm. They depend on the channel geometry and the flow and temperature profile as well. There is some evidence that with increasing superheat and, therefore, more vigorous ejection also the reverse flow and recondensation phenomena are enlarged.

Because of their short duration the energy of even large recondensation pressure peaks is low. Therefore they probably will not cause subassembly destruction and fast failure propagation. Because recondensation and its effect on core structure depends on the complicated geometry, a refined theoretical analysis is impossible at present. For the final proof experiments in multirod and even multi-subassembly geometry are needed.

3. Steam [1,3,31,41,45,46,47,48]

3.1 General Cooling Conditions

The main reason for steam cooling is the possibility of a direct cycle with components more or less based on light water technology. I shall not discuss this aspect but confine myself strictly to the core. Here steam is not as good a coolant as sodium for two reasons:

a) It is a gas,
b) it moderates.

For not exceeding a given clad temperature with gas we now have to consider a not negligible temperature difference coolant to wall. With other words: We have to worry about heat transfer.
Fig.9 shows the typical buildup of temperatures for steam and He compared to one of the sodium cases of fig.6.
The mixing and similar influences are of the same order, whereas $T - \vartheta$, the clad to coolant temperature difference has become much more important (e). Also the hot-channel influences on heat transfer become important (f). The maximum clad temperature then occurs no longer at the channel exit. Therefore, the hot spot is in the region of larger heat fluxes leading to an increase of ΔT in the cladding (g). For saturated steam at the core inlet (direct Loeffler cycle) now the inlet temperature is determined by the steam pressure.

All these additional conditions reduce the degree of freedom for the choice of variables.
In general we have to have among less important conditions to restrain the following 3 parameters within a certain range of values:

$$T_{clad} - \vartheta$$
$$\Delta \vartheta$$
$$\Delta p$$

by 3 variables

G (flow rate)
F (channel cross section)
h_c (core height)

For sodium the first parameter ($T_{clad} - \vartheta$) is unimportant, therefore one of the variables (for instance h_c or F) is independent and may be chosen for maximum breeding, minimum fuel cycle costs or low void effects. For gaseous coolants there is a strict interconnection and no free variable. Also the resulting Δp is of greater importance, whereas for sodium the pressure drop is limited by some not strictly defined structural conditions, for gaseous coolants it determines the pumping power and, therefore, the efficiency to a very great extent.

Now considering the moderating qualities of steam we get another condition on our variables: The channel area F and, therefore, the steam fraction in the core has to be kept at a minimum. This is given by design and tolerance levels in the subassembly at a coolant volume fraction in the range of 30 °/o or a pitch to diameter ratio of 1,15 (in the case of a pin diameter of 7 mm). Therefore, for steam cooled fast reactors there is a unique interconnection between

$$\frac{T_{clad} - \vartheta}{\Delta \vartheta} \quad \text{and} \quad \frac{G}{h_c} ,$$

whereas F and as a consequence Δp now are fixed by other reasons. A certain desired steam exit temperature will result in a certain core height or a certain h_c/d_c and vice versa. We shall point out this strict intercoupling later when looking at the numerical results.

The fuel element is determined by the low coolant fraction and the heat transfer requirements. Grid spacers would raise a large pressure drop. The most favourable solution are here spiral wire or spiral fin spacers (fig.10). The spiral spacers will also help in coolant mixing between neighbouring channels (but not across the whole subassembly) /¯ 24,25,26 _7. This mixing will peel off about 4,5 °/o/cm of the coolant mass flow and reduce the hot channel factor for $\Delta\vartheta$ from 1,60 to about 1,31.

An improvement in heat transfer can be obtained by turbulence promoters, especially of the boundary-layer-type. Measurements on their effectiveness have been made for tube /¯27_7 and rod geometries /¯28_7. Exact values have not yet been established for combinations of spiral spacers of different pitch and boundary layer turbulence promoters. The best present data for turbulence promoters are:

Rise of friction coefficient by a factor of 5 ,
(taken into account the corrosion abrasion to the steam flow)

Rise of heat transfer coefficient by a factor of 2 .

This allows for a substantial efficiency increase. But, since $T_{clad} - \vartheta$, $\Delta\vartheta$, G and h_c are strictly coupled because of the fixed and small flow area, the use of turbulence promoters must result in a smaller h_c. Physically this means that to make use of the higher heat transfer numbers with turbulence promoters, one has to overcome the larger friction and Δp by lower core heights. So for steam the helpful use of turbulence promoters leads to very flat cores with low internal breeding and large numbers of fuel pins. This gives an increase in fuel cycle costs.

The coolant pressure is another important choice. The Karlsruhe work [2] shows a decrease of capital costs with increasing pressure, whereas fuel cycle costs first decrease (gain in efficiency with pressure), then again increase (loss in breeding ratio). Therefore a minimum of power generating costs is around pressures of 150 bars. Others [1,29] have not found as distinct a pressure dependence or favour even quite low pressures [46]. Some proposals, therefore, use supercritical pressures [31,41,48]. They may have low capital costs, but cannot be considered to be breeders.

3.2 Maximum Clad Temperature

In considering all cooling conditions we finally have to fix the maximum clad temperature. While for sodium the internal fission gas pressure defines the design temperatures with respect to cyclic creep deformations, for gas and steam cooling it is the external coolant pressure with respect to creep collapse. Creep collapse is the creep induced oval deformation of the clad tube and is most dangerous for the fresh fuel rod. Later the built up fission gas pressure will compensate this effect.

A theory on creep collapse of empty tubes has been developed by Hoff [32]. Experimental results [33] check with this reasonably well. Fig. 11 gives results for 7 mm o.d. tubes of Inconel 625 and Incoloy 800. At cladding temperatures of 700°C, which are necessary for a reasonable efficiency, even Inconel 625 tubes must be considerably inflated with gas to withstand the coolant pressure.

The fuel will definitely give a certain support to the clad. But with fuel surface temperatures in the order of 800°C most of the fuel is in the plastic range (T > 900°C). The actual strength of support is unknown and certainly limited. No experimental information is available to this point so far.

Therefore a conservative design must use Inconel 625, a "stronger cladding" with respect to creep collapse. Inconel 625 is a relatively strong neutron absorber because of its content of Nickel and Molybdenum (table 2). A more optimistic designer will use a "weaker cladding", either supported by the fuel or by artificial gas pressure ("blow up") of the Incoloy type with lower neutron absorption. The alloy Sandvik 12 RX 72 finally has a composition similar to Incoloy 800, but may allow for higher temperatures. So even turbulence promoters may be unnecessary, thus allowing for a larger h_c [34].

3.3 Summary of Steam Cooled Reactor Problems

Summarizing the problems of steam cooled fast reactor core design today we have the following situation:

a) Clad temperatures are determined by creep collapse. An economic design with good breeding must rely on fuel support, the amount of which is unknown; or extra internal pressurization with all the operational implications is necessary.

b) If strong clads like Inconel 625 are necessary, a loss in breeding ratio is unavoidable.

c) The moderating qualities of steam require narrow coolant channels with the implication of higher hot channel factors and the effects of structural swelling.

d) For good heat transfer at low pumping power turbulence promoters are necessary. Together with the preceding condition this requires flat cores leading to higher fuel cycle costs.

The most recent analysis of the potential of steam cooling has been made within the ENEA working group [34]. Table 3 summarizes the most important results.

Table 3

Cladding	h_c/d_c	BR	η	fuel cycle costs (mills/kWh)	total power generat.costs (mills/kWh)	degree of optimism
Inconel 625 700°	0,198*)	1,12	0,371	1,32	3,82	conservative
Incoloy 800 700°	0,198*)	1,19	0,371	1,10	3,60	optimistic
Sandvik 12 RX 72 735°	0,347**)	1,21	0,362	1,00	3,50	more optimistic

*) clad with turbulence promoters

**) smooth clad (ENEA Reference Design)

The technical and economical ground rules are outlined in the original document. The Pu-α values are based on recent ORNL data determined by Gwin et al. Using the more pessimistic values of Schomberg [35,36], a small breeding decrease is to be expected.

The direct cypital costs have been estimated to be 125 - 130 $/kW for a 1000 MW plant. On this basis the power generating costs of Table 3 have been calculated.

The results of the USAEC evaluation of alternate coolant fast breeder reactors [44] based on the older Karlsruhe D1 design and Babcock and Wilcox proposals are somewhat less favourable. The power generating costs for sodium cooling are in the range of those in Table 3, but breeding ratios are larger.

The main problem of the steam cooled fast reactor is the fuel element behaviour. Statistical testing in a fast flux and steam environment is essential. Since testing reactors are neither available nor under construction, it seems difficult to keep up in time scale with the more advanced sodium line. Since the economic potential of steam cooling does not exceed the potential of sodium (and is inferior to sodium cooled reactors with carbide fuel) it is still under consideration if it is justified to spend the necessary development effort.

4. Gas

The first proposal on a He-cooled fast breeder has been made in 1961 by the Karlsruhe group [42]. More detailed work has been published at the 1963 Argonne Conference [38]. During this time General Atomics started their own work [22,37,39,40], whereas the Karlsruhe interest shifted to sodium and steam cooling. Later Swedish [1], German [30] and UKAEA groups took again a strong interest.

Conditions for gas-cooling of rod type metal clad fuel elements are similar to steam cooling with a few very important exceptions:

a) He or CO_2 moderate only slightly, therefore, the breeding ratio is larger and the void coefficient less positive.

b) The channel cross section or coolant fraction can be varied, therefore, there is no strict coupling of the h_c/d_c ratio to $\Delta\vartheta$. This again allows a broader range of core geometries to choose low fuel cycle costs.

c) Future designs even may use a vented fuel element [66], where the internal pressure is equal to the coolant pressure. This solves the problem of creep collapse, which is otherwise the same. Hence, cladding temperatures of 770°C seem to be possible even with a 316 SS material.

On the other hand here the indirect cycle is indicated with a possible penalty in capital costs. While reloading of the steam cooled core is simple in the flooded condition, here complicated loading machines are required.

The USAEC as well as the ENEA have evaluated gas-cooled reactors too [67,68]. These studies were based on work performed by GGA, AB Atomenergi Sweden, UKAEA, Belgonucleaire and GfK Karlsruhe.

Table 4 shows some of the results of the ENEA study.

By employing a direct cycle with gas turbine the capital costs may be lowered to about 120 $/kW or power costs of 3,1 mills/kWh. This agrees in the range of accuracy with the US study.

Table 4

Fuel type	Cladding	BR	η	fuel cycle costs (mills/kWh)	capital costs ($/kW)	power costs (mills/kWh)
Oxide, sealed pin	Sandvik 12 R 72 HV 730°C	1,50	0,40	0,76	138	3,43
Oxide, vented fuel pin	Stainless steel 316 769°C	1,51	0,41	0,76	138	3,43
Oxide, coated particles	Silicon carbide	1,27	0,42	0,91	132	3,48
Comparison with ENEA steam cooled ref.design						
Oxide, sealed pin	Sandvik 12 RX 72 735°C	1,21	0,362	1,00	126,4	3,50

On this basis there seems to be a small cost advantage for the gas-cooling. However, it has been pointed out by the ENEA working group that the latest steam-studies, aimed at a prototype, are already more refined than the gas-studies. For instance our group has recalculated the second reactor of Table 4 with hot channel factors consistent with those in use for steam cooled reactors /‾69_7. It turned out, that for the same clad hot spot temperature of 769°C the nominal temperatures had to be much lower. This caused the efficiency to drop to 36 °/o. *) Without putting too much importance to this it points out the strong dependence on the hidden assumptions. Therefore the estimated power costs only can define a certain range.

I should conclude that only the potential of the direct cycle solution at present might justify the development of a gas-cooled fast reactor besides the running effort on the sodium line. Therefore, the feasibility of this concept should be evaluated more carefully.

*) The same result has been achieved by the USAEC Alternate Coolant Task Force Study /‾68,page 1.9_7 if the hot channel calculation is based on an "overpower" of ≈ 10 °/o (due to neutron flux distortion by control rods and uncertainties in the power measurement).

5. Conclusions

5.1 Sodium is the coolant with the best cooling capacity, and by far the most experience exists on this material. The remaining problems of economic components and reliable engineered safeguards are of no principal nature but a matter of experience. Therefore, in any economy sodium reactors should be pushed forward, because only then this experience can be gained. In this connection personally I do not so much believe in experience of test facilities but of actual prototypes. Only there the real problems can be defined and solved.

5.2 Steam has suffered some reduction in prospects. Still a gain in power cost can be reached compared to light water reactors. However, the fuel element is an open problem. Therefore, it is still open, whether cost-effectiveness considerations will justify an expensive development program.

5.3 Gas has a very good future outlook with respect to breeding, safety and costs. But presently not too much is known. Gas should be understood as a long range undertaking.

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	Tolerances and uncertainties	Coolant temperature rise	Heat transfer coefficient	Rod power
	Dimensions	1,05	1,02	1,00
	Fuel density	1,02	1,00	1,02
	Fuel composition	1,02	1,00	1,02
	Flow distribution	1,05	1,03	1,00
	Local flux perturbations	1,05	1,00	1,05
Statistical factor	(97,7% confidence level)	1,093	1,036	1,057
	Macroskopic flux distribution	1,07	1,00	1,07
	Heat transfer	1,00	1,15	1,00
	Thermal power evaluation	1,06	1,00	1,06
	Miscellaneous	1,03	1,00	1,03
Absolute factor		1,17	1,15	1,13
Summary		1,28	1,19	1,20

Tab. 1 Hot channel factors (semistatistical) for Na-cooled cores

Alloys	max. content [weight%]										Absorption cross section for fast neutrons of 100 keV [m barns]
	C	Fe	Cr	Al	Ti	Si	Ni	Mn	Mo	Nb	
Incoloy 800	0,1	38,2	23	0,6	0,6	1,0	35	1,5			8,73
Inconel 625	0,1	5,0	23	0,4	0,4	0,5	56	0,5	10	4,1	17,06

Tab. 2 Composition and absorption cross section of several structural materials for steam cooled fast reactor [8]

	Among others function of	Among others function of	
Power generating costs	Burnup Bu	Inlet temperature	ϑ_1
		Temperature rise	$\Delta\vartheta$
		Core height	h_c
		Core diameter	d_c
	Rating r	Total channel length	h_{tot}
		Plenum length	h_p
		Rod diameter	d_r
	Breeding ratio BR and other fuel cost parameters	Clad wall thickness	s
		Coolant channel area	F_c
		Pressure drop	Δp
		Fuel density	ρ_F
		Hot channel factors	H
	Efficiency η	Max. clad temperature	T_{max}
		Rod power	q_r
		Enrichment	e
	Plant costs C		

Fig.1 Cost dependence on „internal" or core parameters

	Among other functions of	Among other functions of
Power generating costs	Plant costs C Efficiency η	Heat exchanger Steam generator Turbine Generator Pumps Valves Safety Building

Fig. 2 Cost dependence on „external" or circuit parameters

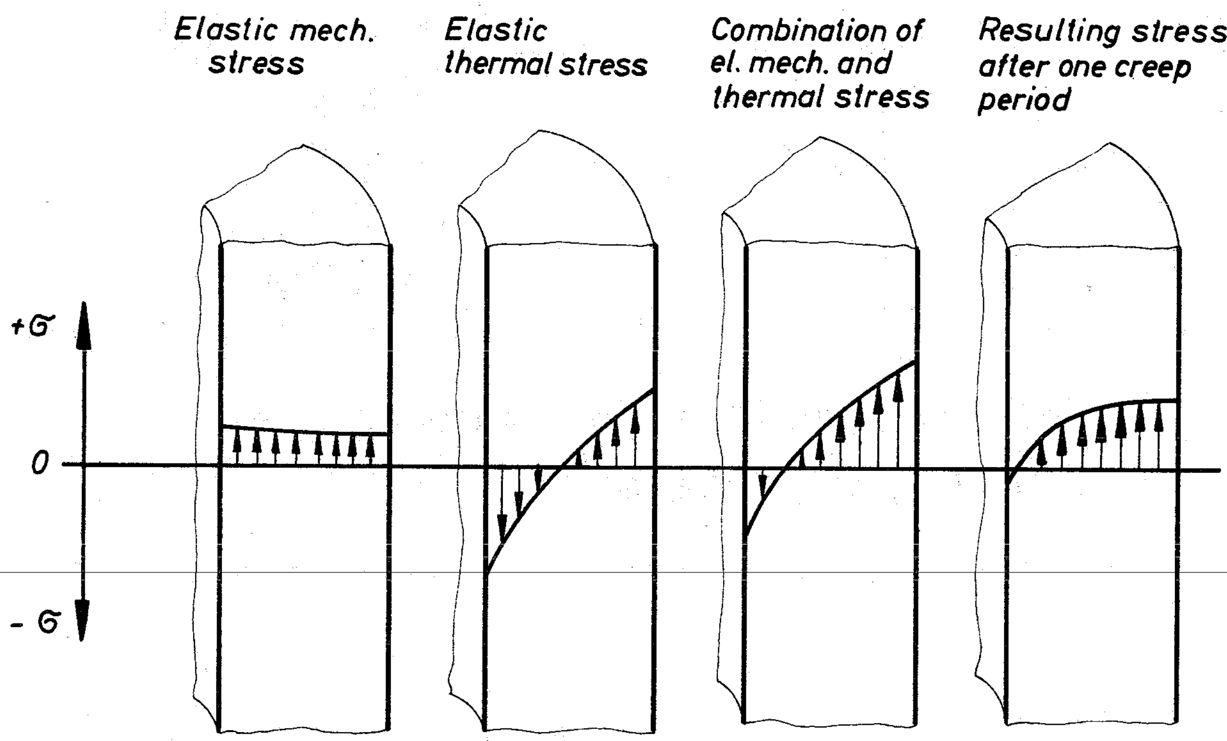


Fig. 3 Combination of mechanical and thermal stress in Fast Reactor claddings

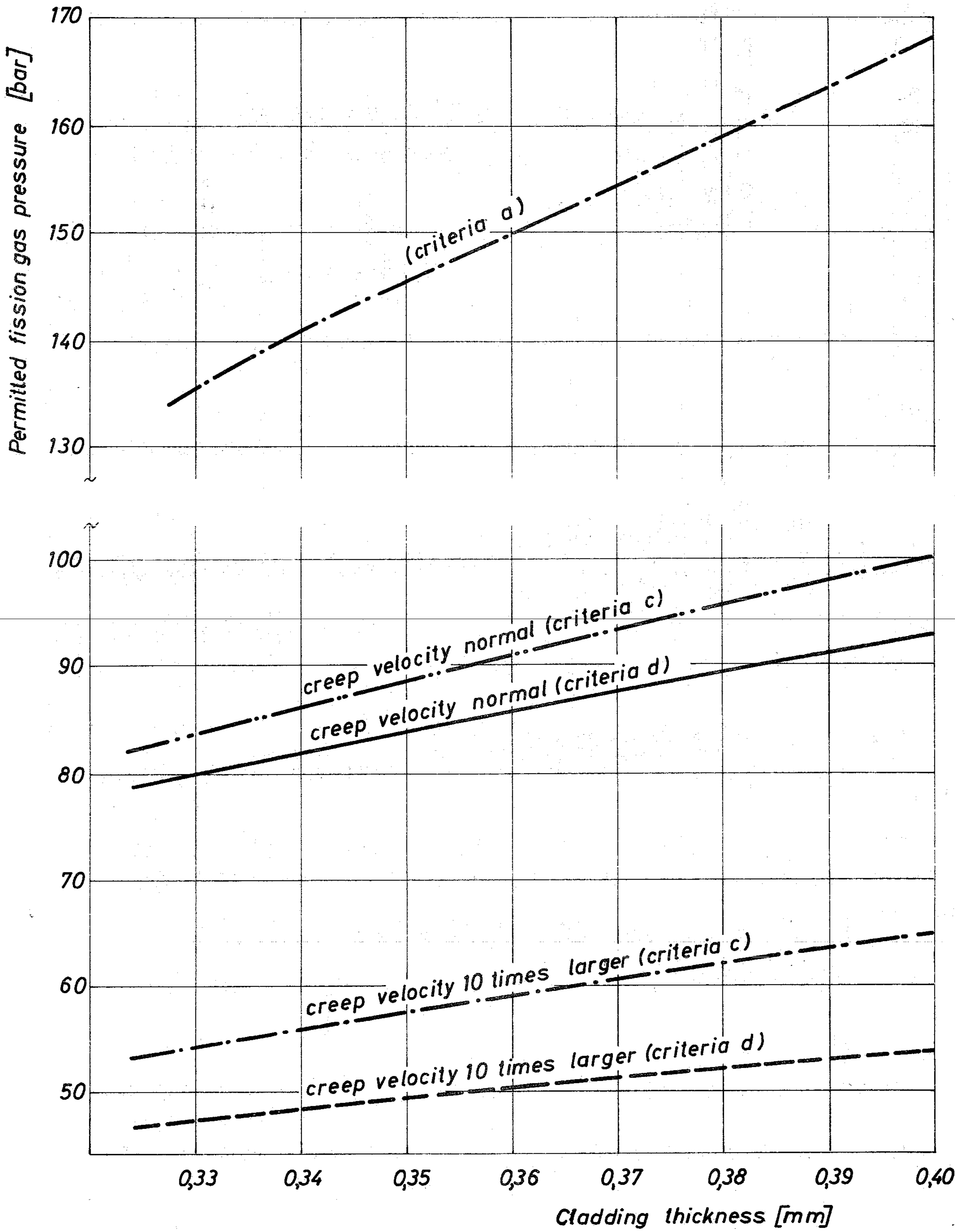


Fig. 4 Permitted fission gas pressure for a X8 Cr Ni Mo V Nb 16 13 cladding at 700 °C

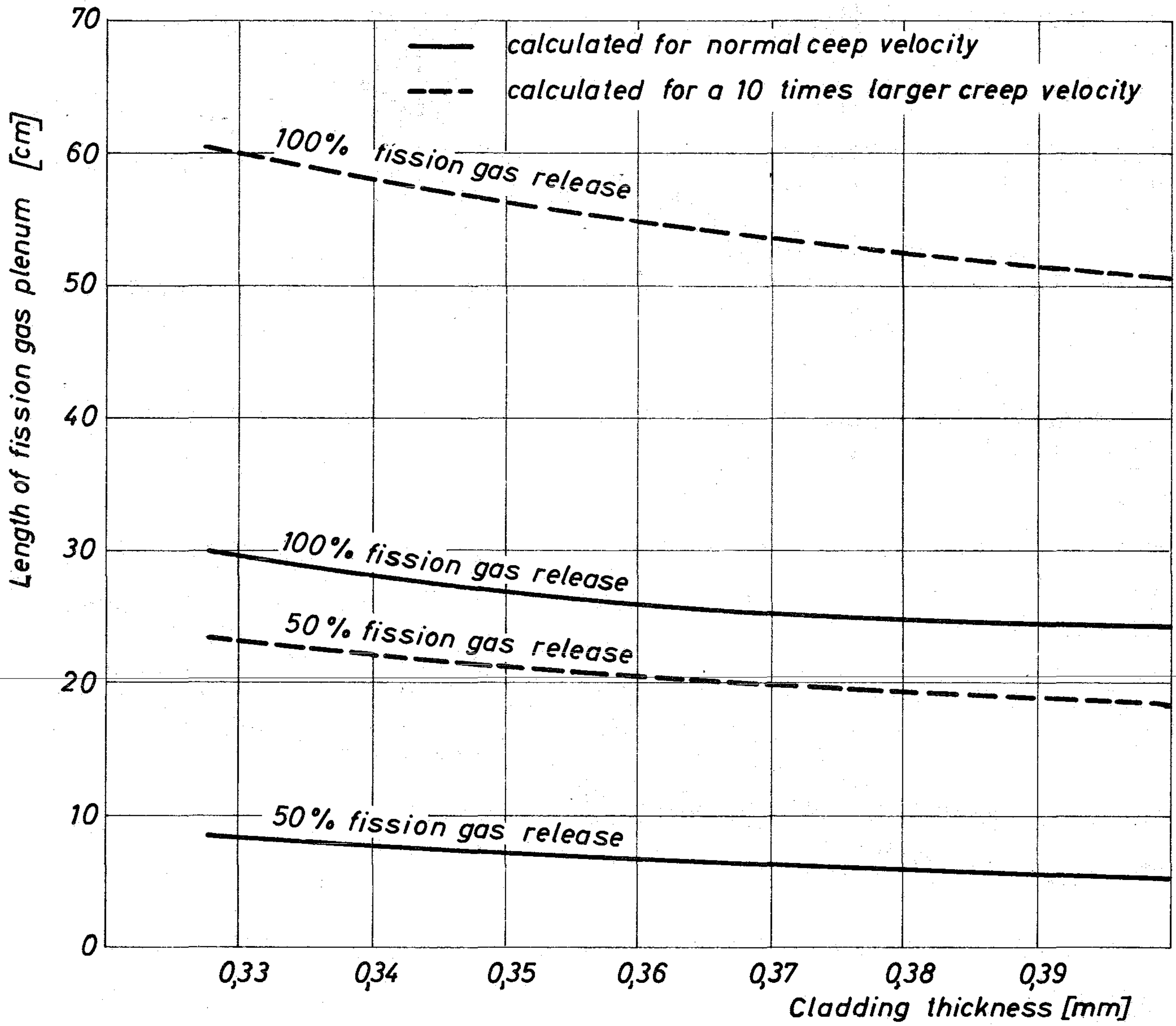


Fig. 5 Length of fission gas plenum as a function of wall thickness

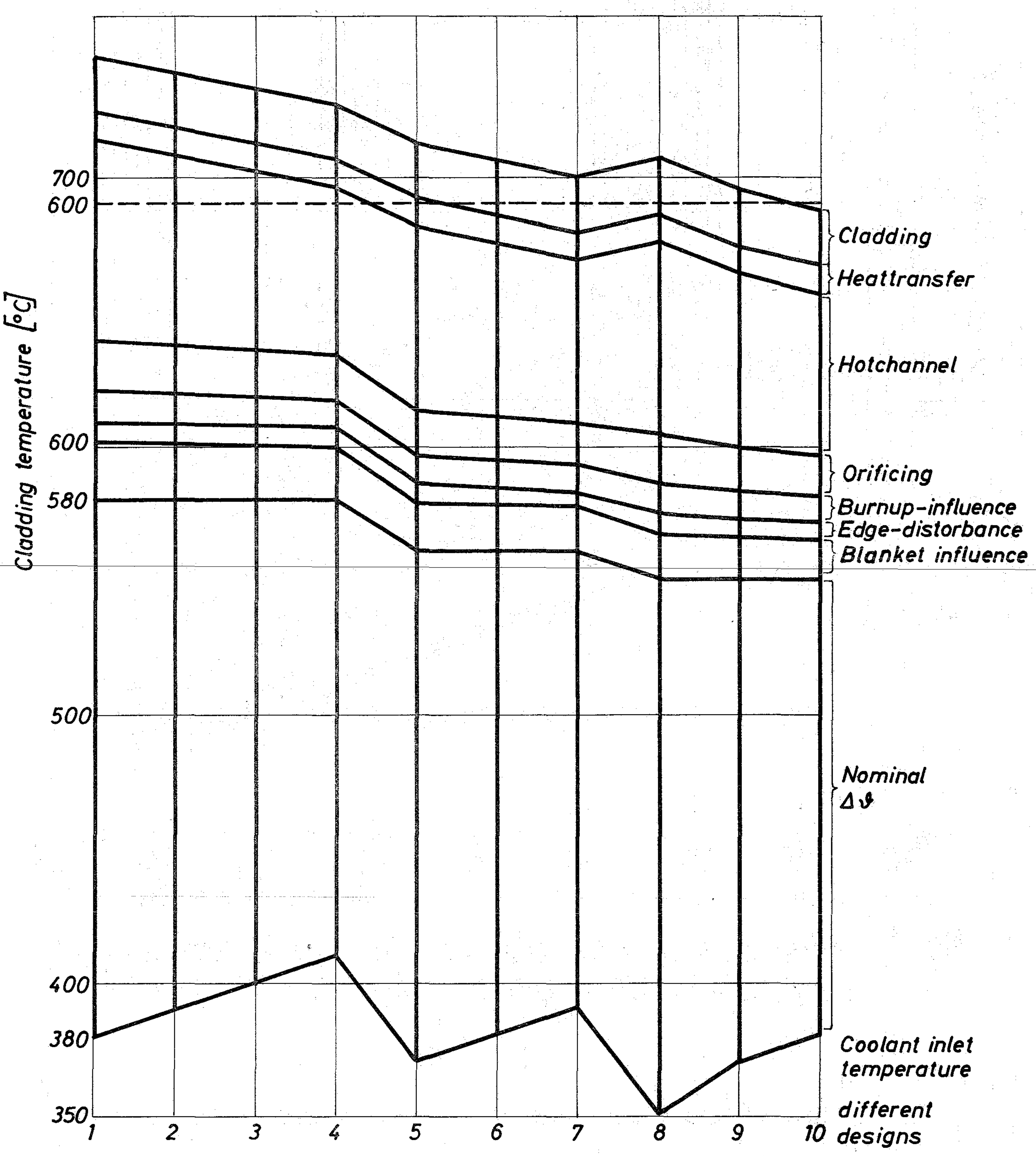


Fig.6 Temperature buildup for different sodium cooled designs

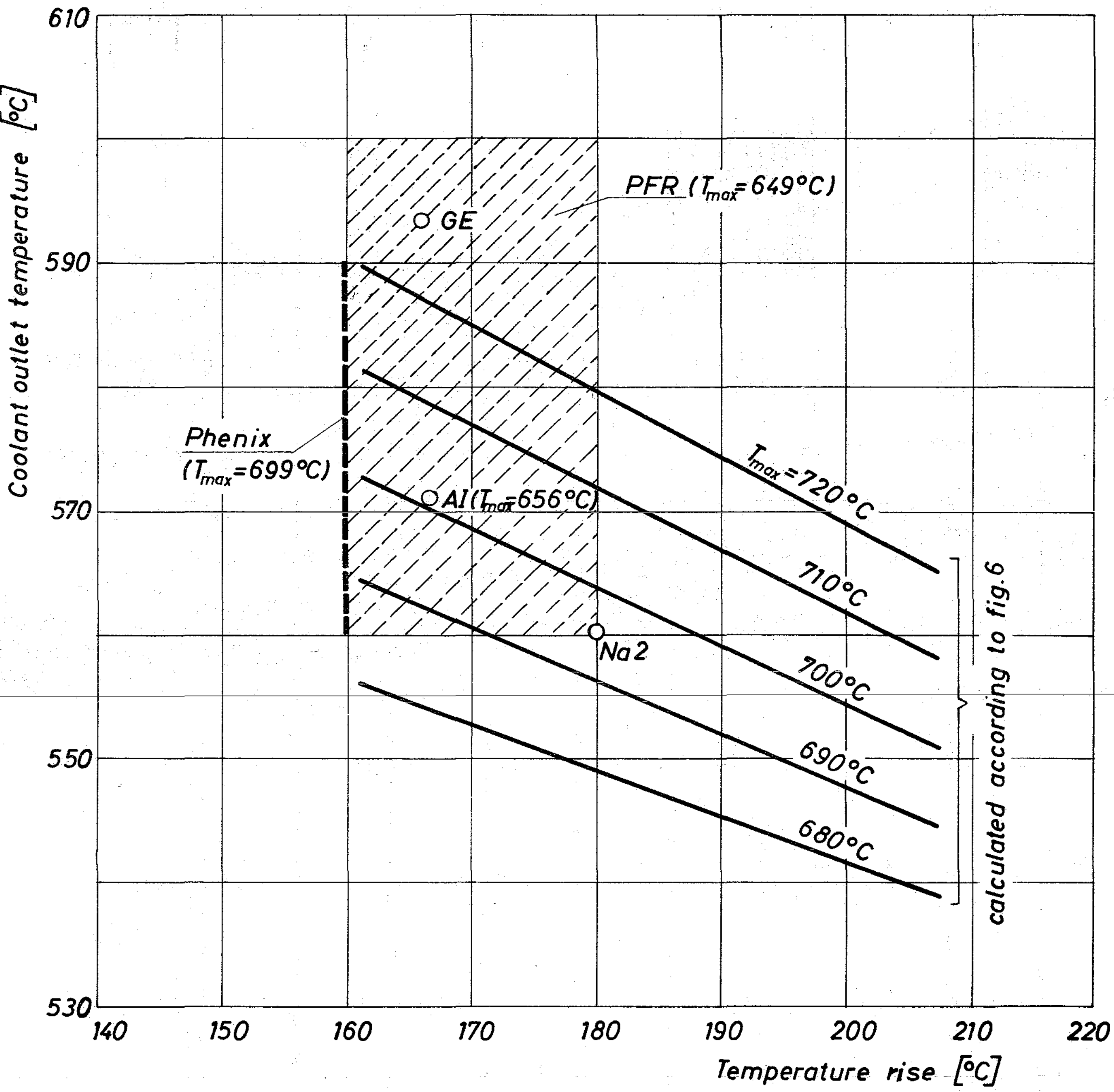


Fig. 7 Maximum clad temperature as a function of coolant outlet temperature and temperature rise

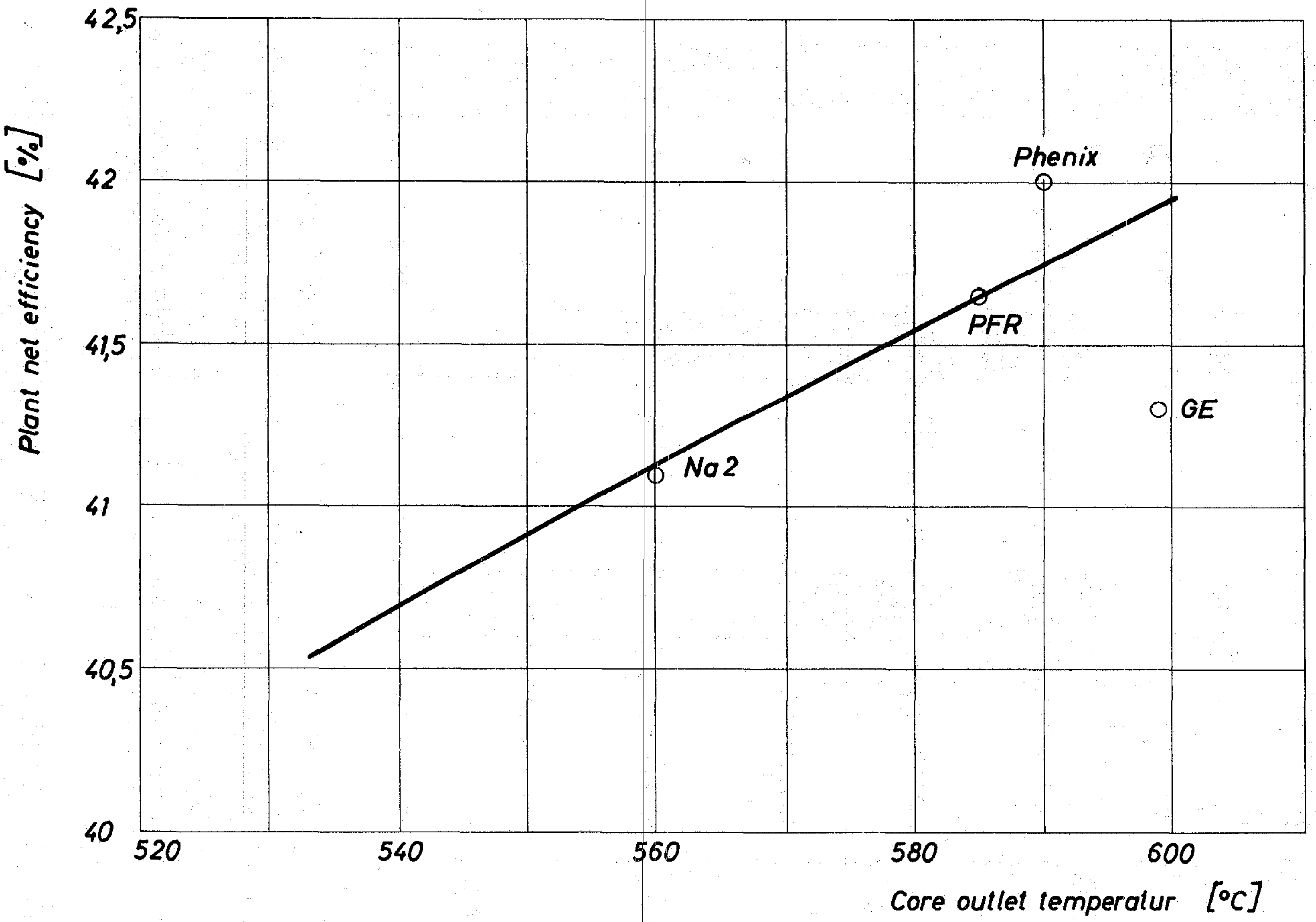
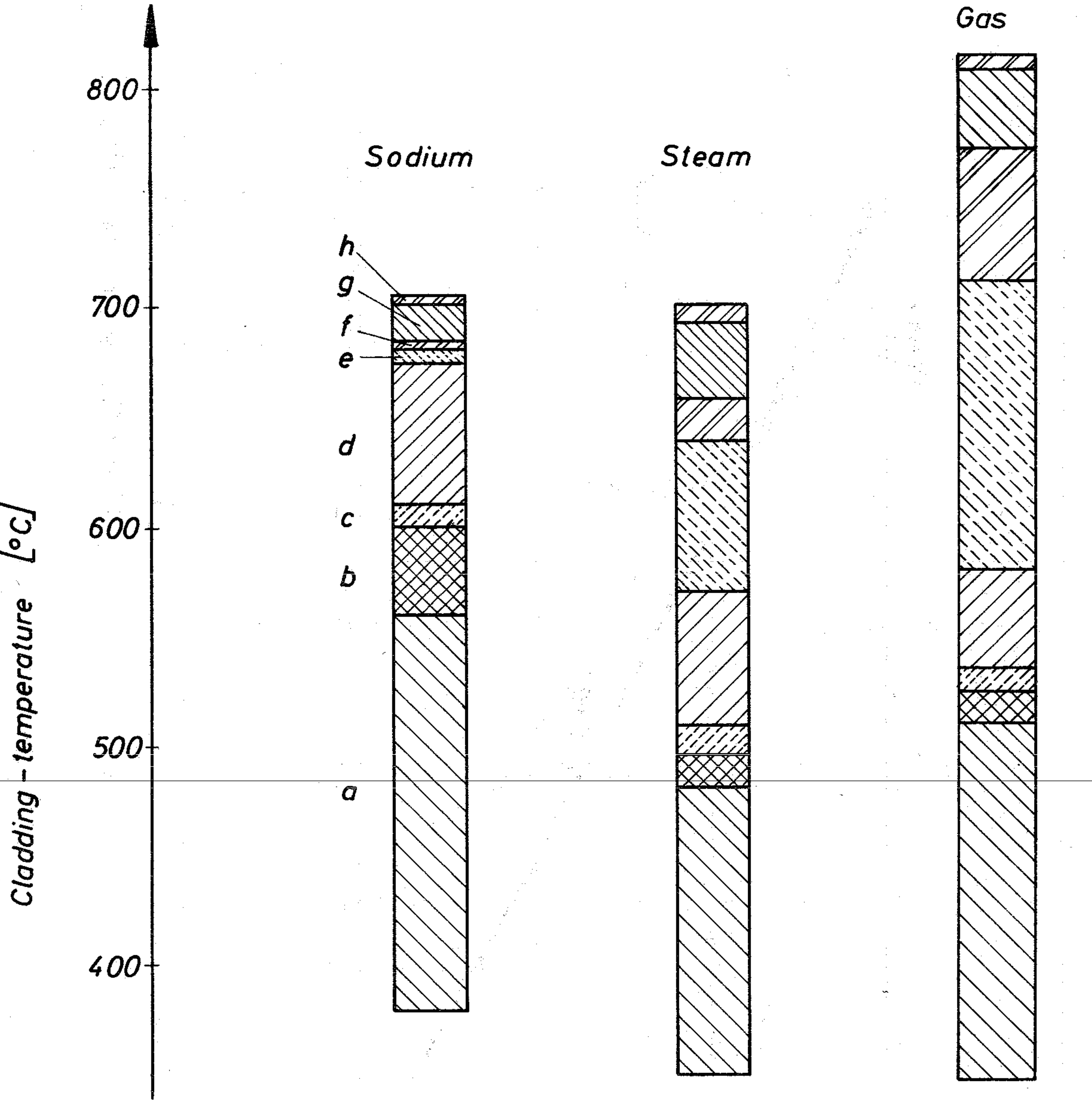


Fig. 8 Approximate plant net efficiency as a function of sodium core outlet temperature



a Nominal $\Delta\vartheta$

b Blanket-, orificing-, leakage-influence and edge disturbance

c Burnup influence

d Hotchannel influence on $\Delta\vartheta$

e Heattransfer

f Hot channel - influence on heattransfer

g Heattransfer in cladding

h Hotchannel - influence on cladding heattransfer

Fig.9 Cladding temperature buildup at location of hotspot temperature for gas and steam cooling compared to sodium cooling

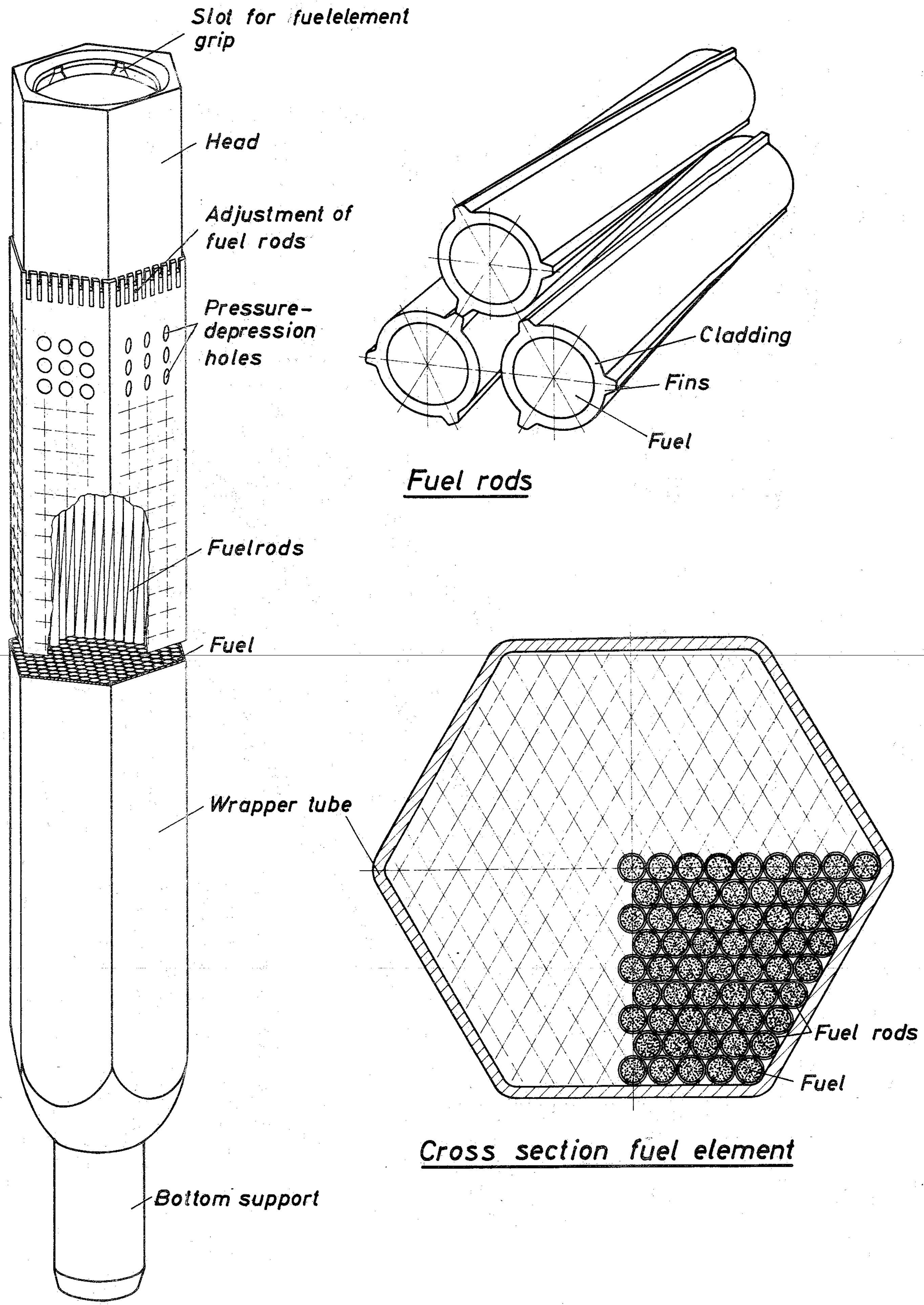


Fig.10 Fuel element of steam cooled reactor

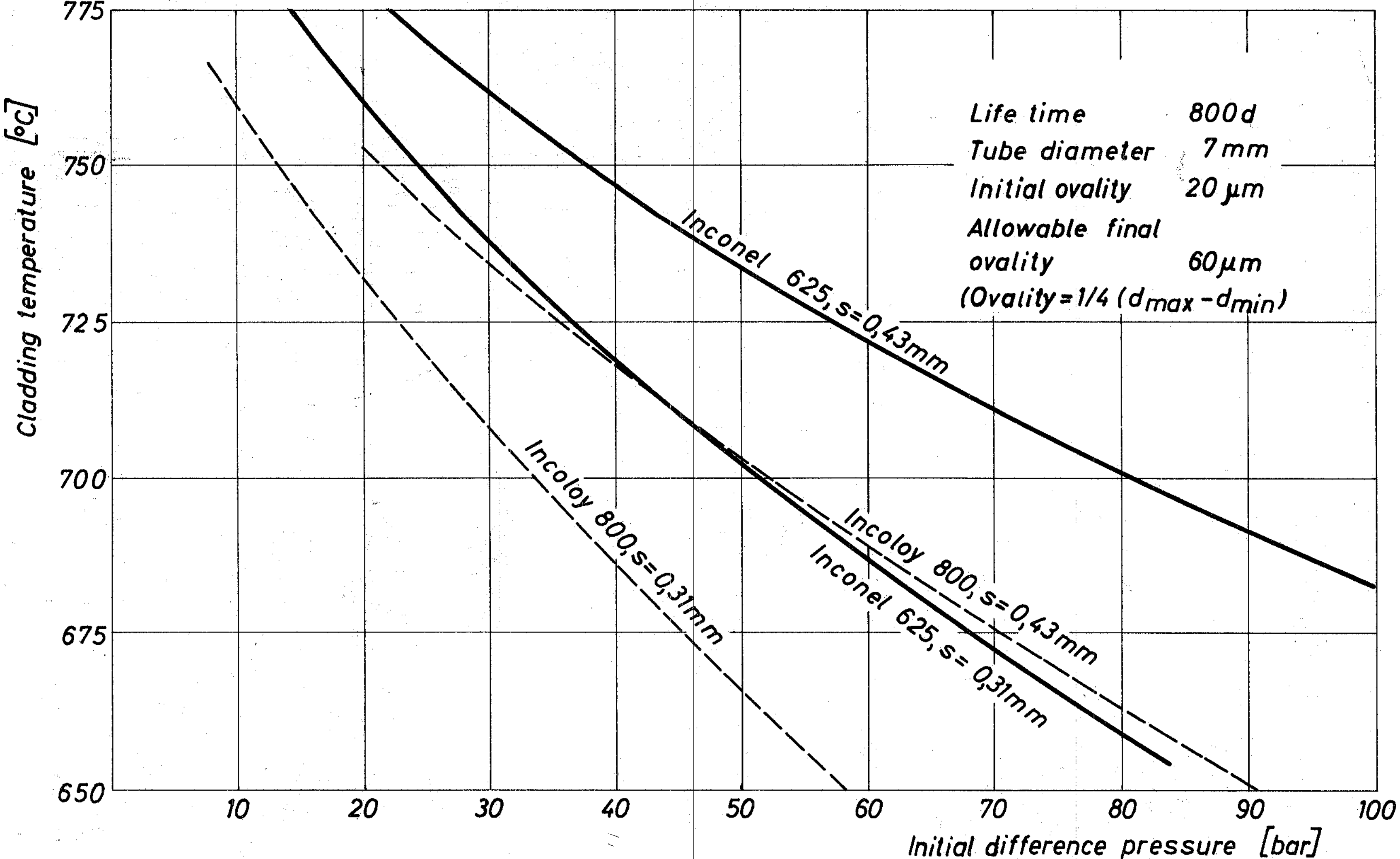


Fig.11 Cladding temperature as function of the initial pressure difference outside-inside of the cladding (at hot conditions) for Incoloy 800 and Inconel 625 as a result of creep collapse calculations