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of
**GAS-COOLED REACTOR
INFORMATION MEETING**

at the
OAK RIDGE NATIONAL LABORATORY
April 27-30, 1970
Oak Ridge Playhouse
Oak Ridge, Tennessee

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FOREWORD

Helium-cooled nuclear reactors offer potential for meeting the major long-term requirements for electric power generation. The reactors fall into two categories: first, the High-Temperature Gas-Cooled Converter Reactor (HTGR or HTR) that uses graphite for the fuel structure and moderator; and second, the Gas-Cooled Fast Breeder Reactor (GCFBR or GCFBR) which has a core composed of metallic-clad pins of mixed uranium-plutonium oxide fuel. Both reactors can be used with either a direct turbine cycle or a modern steam cycle for energy recovery to drive the electric generator.

Major incentives for application of the HTGR are the prospects for economically competitive power production while utilizing more effectively the uranium ore reserves. The excellent neutron economy and fuel performance make possible high burnup through which low fuel-cycle costs can be obtained and maintained as uranium ore costs rise. The high-temperature capability of the graphite core structure facilitates operation at high thermodynamic efficiencies, and thus requirements for heat dissipation to the environment are low. Excellent retention of fission products and other radioactive materials appears to be achievable by the coolant circuits housed in concrete pressure vessels.

The helium-cooled GCFBR offers a high breeding ratio with a fuel doubling time of less than 10 years, a core conversion ratio of approximately 1.0, a small reactivity change from loss of coolant, a potentially nonradioactive circuit, and high thermal efficiency. The principal questions concerning the concept center around the high-pressure coolant, particularly for afterheat removal following loss-of-cooling accidents, and the performance of metal-clad ceramic fuel elements for relatively high temperature operation, approximately 700°C.

Development of gas-cooled reactors for electric generating power stations has been under way in the United States since 1956 and for a longer period in several European countries. Four helium-cooled reactors have operated — Peach Bottom, in Pennsylvania, the AVR at Jülich, Germany, the Dragon Reactor in the South of England, and UHTREX at Los Alamos. Three of the reactors represent early concepts for helium-cooled nuclear electric power stations. The fourth, UHTREX, was modeled for direct use of heat. The operating experience with these reactors provides a broad basis for new designs and contributes greatly to the projection of HTGR performance.

The Oak Ridge National Laboratory holds periodic meetings for the reporting of its work in gas-cooled reactor development. This meeting constituted an extension to provide greatly increased participation by others. In effect, the meeting was conducted as an international symposium at which comprehensive papers were presented on all aspects of gas-cooled reactor technology. The papers and discussion at the meeting provided an opportunity to review the total development program, the design concepts, and the operating experience from gas-cooled reactors with emphasis on the HTGR which is now entering the commercial field. An

important session at the meeting, however, was that on the gas-cooled fast breeder reactor held in conjunction with the Molten-Salt Breeder Reactor meeting. Here considerable advances in the study and analysis of safety problems for the GCBR indicated more clearly its feasibility and practicality. Direct helium turbine cycle systems were described as having longer-term potential for reducing capital costs.

In the presentation of these proceedings the Oak Ridge National Laboratory wishes to thank all of those who participated in the meeting. Particular appreciation is expressed to the authors, the session chairmen, and panelists who were most cooperative in providing abstracts, texts of papers, and discussion comments so that the timely preparation of these proceedings would be possible.

D. B. Trauger

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Introductory Remarks

H. G. MacPherson

Welcome to Tennessee and to the Oak Ridge National Laboratory. We are deeply honored and pleased to play host to such a distinguished gathering of experts on high temperature gas-cooled reactors. If there is anything we can do to make your stay here more pleasant, please let us know.

Since this international meeting on gas-cooled reactors is being held in Oak Ridge, I think it is appropriate to pay tribute to the early efforts of Farrington Daniels. Professor Daniels had all the right sentiments and motives to win approval of this audience - the only trouble was that he was active about 10 years too early. In 1944, in what was then the Metallurgical Laboratory, now the Argonne National Laboratory, he proposed a pebble bed reactor to use a mixture of graphite and uranium carbide pebbles or balls. This was a rather advanced proposal according to present day standards. He suggested such things as operation at 2000 °C, a reactor diameter of 35 feet to operate on natural uranium, cooling with either He or boiling bismuth, and distilling Pu and fission products for the reactor during operation.

These ideas led to the authorization of the Power Pile Project in 1946. This was a helium-cooled BeO-moderated reactor, called the Daniels Pile, which was to be built in Oak Ridge. Although this reactor project was canceled before it was two years old, the group of engineers that was assembled for this purpose went on to build the U. S. submarine reactor.

Farrington Daniels did not give up, despite the official discouragement. He modified his ideas, and made a specific proposal of a more simple graphite-moderated gas-cooled reactor in 1950. This reactor proposal was very specific for the operation of a gas turbine using N₂ as a coolant, and he even went to the trouble of locating available gas turbines and getting quotations on their delivery. This proposal fell on deaf ears, but back at the University of Wisconsin Daniels continued his research activities.

In 1955 we had some correspondence at a time when he was seeking a method of sealing uranium fuel tightly in small pieces of graphite. I was then with the Carbon Products Division of Union Carbide Corporation, and we provided him with our then best effort at a small sealed graphite capsule. This was not quite good enough, but Daniels did publish a paper in 1956 proposing a helium-cooled reactor based on this fuel. After that his major interest turned to solar energy as a heat source for backward countries.

It is too bad that Daniels' enthusiasm and effort had to come 10 years too soon, both from a political and a technical point of view. The world was not ready for commercial nuclear power when he started, and he was not able to persist long enough to be rescued by the development of pyrolytically carbon-coated fuel particles, as the rest of you have been. It is my personal opinion that the discovery of true fission product retention by pyrolytic coatings is the most important single advance in gas-cooled reactor technology since Farrington Daniels started in 1944, in that it makes gas-cooled reactors credible from a commercial point of view.

REACTORS IN OPERATION

(Session I)

Chairman: J. S. Kemper,
Philadelphia Electric Company

Co-Chairman: S. I. Kaplan,
Oak Ridge National Laboratory

DRAGON OPERATING EXPERIENCE

B. G. Chapman
OECD Dragon Project

0957000

ABSTRACT

The use of Dragon as a fuel irradiation facility has generated a great deal of experience in operational areas such as helium utilisation and engineering, maintenance of helium circuits, activity behaviour in HTR circuits, and the detection and clean-up of chemical impurities. The paper describes this experience over the last five years at Dragon.

It is frequently difficult to predict the operational performance of large plants from the drawing board and the chief value of experience is to establish the practical limits which can be achieved. In this respect Dragon has demonstrated the essential viability of the HTR concept and has enabled realistic design criteria to be laid down.

INTRODUCTION

Dragon was built to prove an idea and the heart of that idea was a reactor core constructed entirely of graphite and ceramic nuclear fuels, and cooled with helium. All other considerations in the design of the reactor experiment were secondary to that of creating the correct environment for the operation of such a core. For this reason many design features were introduced which are not typical of a commercial HTR plant. Nevertheless, in addition to fuel testing, a great deal of experience and information has accrued from the Project to guide designers of future systems.

Since the reactor first achieved full power in April, 1966, four years of development have gone into the operation and maintenance techniques applicable to the HTR. Three fuel charges consisting of 15 different core layouts have been irradiated and some major difficulties overcome. In particular, since the first boiler tube failure in February, 1967, the reactor has been dogged by water-side corrosion troubles - of almost no relevance to the concept of the HTR. Dozens of tubes have been plugged and 13 boiler changes carried out. This has had a serious effect on utilisation. During Charge II utilisation was 52.5% (about 60% of the target set by considerations of fuel changing). For Charge III the corresponding figures are 50% and 68% (Fig. 1)^{1, 2}. There can be little doubt that, without the boiler problem, over 90% of the desired irradiation would have been achieved. The time that has been lost in irradiation, however, has provided valuable experience in maintenance and operating techniques and it is primarily with this aspect that this paper is concerned. Five main areas will be considered.

The Performance of Large Scale Helium
Circuits and Associated Components

The use of an expensive coolant such as helium places circuit leakage uppermost in the designers' mind. The target leakage rate of 0.1%/day, to which Dragon was designed, has become almost traditional - yet it is impossible to relate to design data for components.

The diffusion of helium through metals can be below the limits of detection provided that careful selection and control are exercised. For instance, some leakage may be expected along a rolled bar and small diameter cover plates must be made from rolled plate (in which imperfections are lateral) and not from bar (in which imperfections are longitudinal). Similarly, welds of good standard may be expected to show negligible leakage. Thus most components are tested primarily for imperfections rather than conformity with a design prediction. Since, in the absence of imperfections, the leakage should be vanishingly small, the test specification becomes related to the sensitivity of the test procedure. The Dragon leak testing routines developed by Wade³ are based upon specified mass-spectrometers used either in the vacuum mode (component evacuated in a helium atmosphere) or as "sniffers" (component pressurised with helium or helium/nitrogen mixture). Sensitivity can be increased and the overall testing of large components simplified by "bagging" in polythene and allowing helium build-up in the bag for a specified period. By these means, leakage rates at least two orders of magnitude less than 0.1%/day can be measured.

Turning to joints and seals, it is possible to lay down average performance data from laboratory tests. The silver gasket joints used on Dragon have a typical leak rate of 1×10^{-8} ata cm³/s/ft at 25 atm. There are 600 ft of such gaskets in Dragon leading to a leak rate of 3×10^{-7} %/day^{4, 5}. Small pipe couplings and "Conoseals" have an excellent possible performance but are mostly very sensitive to flexing and thermal cycling. The problem here is that although the leakage from each coupling may be very small, they are used in large numbers widely distributed in space.

From this it can be seen that a target figure of 0.1%/day makes a generous allowance for the frailty of man. It also sets a problem for operators in that leak rates of this order can be very difficult to detect, let alone measure. The usual routine in gas cooled reactors is to carry out regular coolant balances. This requires that all vessels, pipes, etc., be provided with pressure and temperature

monitoring so that gas contents can be calculated. At Dragon such balances are carried out weekly and although great care is taken with instrument calibration and Van de Waals deviations from the perfect gas laws are taken into account, experience shows that an accuracy of $\pm 3\%$ is typical. In particular the content of the primary circuit, with temperatures ranging from 150°C to 830°C , is difficult to assess. Thus helium balances will not show a 0.1% /day leak rate in under four weeks.

It has been possible, at Dragon, to measure the total unaccountable leakage by use of the containment building. This building is continuously ventilated by an open circuit system with a complete air interchange every 22 hours. The helium in air concentration is measured by a mass-spectrometer which has been calibrated by making measured releases of helium into the building. By this means we find that a leak rate of 0.045% /day gives 100 vpm of helium in air. Fig. 2 shows a typical history from which it can be seen that normal leak rates lie between 0.025 and 0.05% /day. The assessment of human frailty seems surprisingly correct.

Fig. 3 shows the total usage of helium over the same period, computed from helium balances. It is apparent that the unaccountable losses are high compared with those calculated from Fig. 2. There are three reasons for this:

- (a) Losses occur during shutdown when the mass-spectrometer is not used because the containment building doors are open.
- (b) Large leaks (shown by peaks on Fig. 2) are usually corrected before the helium-in-air measurement has equilibrated.
- (c) Usage of helium for chemical analysis is not metered accurately and a notional figure is used in the balances. It would not be economically justified to improve this situation.

The most important conclusion from our experience is that the total usage of helium far outweighs the loss by leakage. In a commercial HTR this will be even more true. Usage in purging on-load refuelling machines and multiple stand pipe closures will be higher than that required for the Dragon machinery and leakage should be less

(expressed as a daily percentage). We find most leaks arise from complex areas such as instrument and analysis small bore lines and not from the large vessels. Since Dragon is very highly instrumented, leakage experience is probably pessimistic.

What lessons can designers learn from this?

- (a) A leak rate of 0.1%/day should be comfortably achievable.
- (b) Helium-in-air monitoring is by far the quickest and most sensitive technique for controlling leakage. If possible, reactor buildings and ventilation plant should be designed to facilitate this.
- (c) Wherever possible, components should be specified on a "no detectable leak" basis. Although this cannot be defended in terms of total leakage it greatly facilitates the maintenance of low backgrounds and simplifies leak detection and location. If the component manufacturing industries are encouraged to think in these terms, the economic penalty need not be significant.
- (d) A low pressure collection system and recovery compressors are essential.
- (e) As much as possible of the plant should be designed for evacuation to minimise purging losses prior to major maintenance.
- (f) A computing data logger capable of producing routine helium balances would help in locating major leaks.

Early recognition of possible lubrication problems in oxygen-free helium led to extensive testing in this area. The design and construction of Dragon however, preceded much of this work and the fact that no bearing seizure has occurred in the primary circuit must be attributed to design rather than any startling new materials. Wherever possible, bearings (always rolling surfaces) are positioned in low temperatures and fluxes, mechanisms are designed to accept high clearances, speeds and total duties are low, and grease packing is used. Experience has shown that sealed greased bearings do not produce significant contamination. Greases are selected for minimum tendency to generate "gummy" residues and later developments include oxygen releasing

additives. Where conditions preclude the use of grease, standard bearings (3 dot-pressed cage) are used after Nital etching and MoS_2 treatment. Bearings have been designed for more arduous duty and details can be found in^{6, 7, 8, 9, 10}. A useful summary of design desiderata is contained in¹¹.

A design of particular interest is the use of oil bath lubrication in the control rod gear-boxes. These boxes are located in cool regions and are connected to the main vessel by pressure tubes which carry the drive shafts. Normal nuclear grade oils, after degassing, perform well, and no foaming has been encountered on depressurisation. This technique would appear ideally suited for PCRV application.

The bearing types referred to are suitable only for light loads, moderate speeds and occasional use. The main circulators clearly require a more radical approach and gas bearings were selected for Dragon, thus avoiding the rotating shaft seal problem. During the 100,000 total machine hours to date, no machine has failed in normal service. Four failures have occurred, all at minimum idle speed (1,500 rpm) in nitrogen at 1 ata. Two of these were considered to be due to deficient jacking gas flow at start-up, one to loss of dynamic balance due to a loose nose fairing and the other to water ingress following a heat exchanger tube failure. In all cases intermittent bearing temperature rises preceded the failure by several hours and the machines ran normally until they were deliberately stopped. They then refused to restart. In addition to the above four machines, two have been stripped for routine examination after about 16,000 hours and found to be in excellent condition. The only servicing required has been minor rebalancing to the specification of 0.5 g cm.

Circulator reliability is usually a central point in the safety analysis of gas cooled reactors. Our experience would indicate that gas bearings have particularly favourable characteristics in this respect. Since the bearing separating forces rise so rapidly with speed, the machine experiences its most difficult test at start-up and idle speed operation. Thus, if a machine starts satisfactorily, we

have confidence in its integrity for normal high speed duty. To test the machine's ability to withstand a primary circuit failure, one circulator was depressurised at 50 psi/s, on a test rig. No bearing touch occurred, indeed, the bearing capacity measurement indicated a negligible change in clearance.

Finally, some mention must be made of diaphragm compressors. These machines have been the cause of considerable difficulty on all current HTR plants. Fig. 4 shows the general improvement in diaphragm life from about 100 hours, 5 years ago, to in excess of 1,000 hours currently. This improvement has been achieved partly by modifications and partly by improved operational technique. As supplied, the machines had triple diaphragms, two mild steel on the oil side and one stainless steel on the gas side. These were assembled dry. It has been found preferable to fit the stainless diaphragm between the mild steel ones and to smear them with oil before assembly. Changes have been made to the interstage pressure controller, the oil and water coolers, and an oil catch-pot with oil detector fitted to the discharge line. A sump oil level gauge to detect oil loss has also been installed. Operational routines have been devised to avoid excessive hammering and stressing of the diaphragms. These include unloaded starting, minimised running against low discharge pressures, pressure relief on shutdown and the avoidance of sub-atmospheric pressures at inlet. As a result of these changes, the performance of the compressors is acceptable although we continue to be interested in more recent developments such as the "roll-sock" machines.

Summarising, our experience with the Dragon helium circuits, I consider that, as the result of conservative design, careful maintenance and monitoring and the steady improvement to components and operating routines, we have now reached the stage of affirming that helium cooling is entirely practicable. I am convinced that the merits of helium cooling can be realised without excessive economic or operational demands.

The Behaviour of Fission Products in the Primary Circuit

The primary circuit activity levels in Dragon are low by almost any power reactor standards. Of course, experimental and exploratory fuels are deliberately protected by the purge system but in recent cores the fraction of fuel purged is small (Charge II ~35% Charge III Core 8 ~24%). Thus the primary circuit activities give a valid indication of conditions applicable to power reactor design. Nevertheless, relatively small differences in fuel particle design, fuel pin geometry and operating conditions can affect activity release strongly - particularly in respect of those isotopes of low volatility and high tendency to plate-out on graphite or metal surfaces. Thus in Dragon Project work, predictions of activity levels in power HTR's are being based on two foundations. Release from power reactor fuel pins is being assessed using theoretical models. The data for these calculations is not being taken from direct measurements of Dragon primary circuit activities. This would be too crude and liable to error. Instead, the detailed information coming from purge gas measurements and studies of fission product distributions in the particles, compacts and fuel tubes of irradiated elements, is used as the basic data. The need for this is further reinforced by the short irradiation times in Dragon as compared with the power reactor.

The other foundation is the use of the Dragon Reactor to study the behaviour of released fission products as they move around the primary circuit. It is this aspect which will be discussed here.

It is possible to affirm now that gas-born activity per se in a HTR will not constitute a problem. That is to say, direct radiation from coolant ducts and pipes will be small. Equally, leaks from the circuit should not present a hazard. There are four vital questions which must be answered (and upon which fuel design criteria will rest).

- (a) What will be the long-term activity build-up on components which need maintenance or inspection?

- (b) How much deposited activity will exist in the circuit which could be released under serious accident conditions?
- (c) What are the problems of decontamination?
- (d) How can failing fuel be best located before serious release results?

The start-point for these studies is the determination of a full activity burden specification for the coolant.

For a number of years, fission product activity levels in the primary circuit were measured either by an installed continuous flow β chamber or by the counting of discrete gas samples on a γ -spectrometer. The β counter was calibrated with Xe-133 and for this reason gas activities are usually expressed as "Xe-133 equivalents". There is no doubt that this method yields a reasonable estimate of bulk gas activity which is valid for estimating radiation levels from ducts, etc. Typical values in Dragon are low, e.g., $1,000 \text{ pCi/cm}^3$ or 0.4 Ci total circulating activity. γ -spectrometry of gas samples rarely showed any detectable activity other than rare gases and for this reason the presence of I-131, Sr-90, Cs-137, etc., in the gas was not suspected. Further confirmation of this view was suggested by the lack of I-131 in boiler water which had been subjected to a flow of primary circuit gas through leaking tubes.

Detailed studies of all boilers and blowers removed from the reactor however, revealed measurable quantities of Iodine, Caesium and Strontium. There was thus no doubt that solid fission products were circulating in the coolant but the original sampling techniques failed to detect them - probably because those activities were deposited on the lines leading to the chambers or sample bottles. A new sampling system known as a "Dracule" (Fig. 5) has therefore been installed which draws a sample of primary circuit gas as close as possible to the core exit. The gas sample is then cooled by passing it through a removable tube with a controlled temperature gradient¹⁵. After removal, deposited activity can be assayed with a γ -spectrometer. A

number of variations are possible. The tube may be fitted with filters or iso-kinetic heads for dust sampling and may have specially prepared activated surfaces, etc. A typical profile for I-131 is shown in Fig. 6 taken from¹⁵. One unexpected discovery is that Xe-133 is quite strongly absorbed on the tube surface.

By use of the Dracule and the gas sampling systems a total gas impurity specification can be derived. Short-term measurements before a shut down can then be related to the deposited short-lived activities found on the surfaces of reactor components removed during the shut down. This is of particular interest for I-131. The assessment of accumulation of long-lived isotopes such as Cs-137 requires a time integration of circuit levels and the installation of new boilers and circulators with known activity levels will provide a suitable base line for this purpose.

Shortly before the shut down for the installation of new boilers, the primary circuit activity was unusually high having a total specification as in Table 1.

As soon as components were removed from the primary circuit they were assayed for deposited activity. The following results for I-131 are typical:

- (a) From the surface of the stainless steel nose cone of the boilers which runs at about 735°C, $5 \text{ to } 10 \times 10^{-2} \mu\text{Ci}/\text{cm}^2$ was removed by methanol strygil.
- (b) From the surface of boiler tubes (Fig. 7) - mild steel at about 205-235°C - $10\text{-}20 \mu\text{Ci}/\text{cm}^2$. The levels of iodine do not appear to vary much with the angle of incidence of the gas. This supports the view that the iodine is in gaseous rather than particulate form; Deposition of isotopes such as Cs-137 is strongly dependent on the gas flow direction.
- (c) From the blower impellers - alloy steel at about 330°C - on the trailing faces of the vanes $20 \mu\text{Ci}/\text{cm}^2$ and on the leading faces $2 \mu\text{Ci}/\text{cm}^2$. This difference is probably accounted for by the

significantly greater dust deposition on the trailing faces.

- (d) On the Nimonic-75 coolant duct running at about 740°C no I-131 could be detected. The main activity was Cs-137 at $0.14 \mu\text{Ci}/\text{cm}^2$. This value is of interest since it represents a situation similar to that of a direct cycle gas turbine blade.

The methods used to remove activity for assay provide some clues to the problems of activity release under accident conditions and the difficulties of maintenance and decontamination. ¹⁶ describes the results obtained with methanol and phosphoric acid stryglis and the "electroleech". In addition plastic (BEX) film has been used. Perhaps the most significant fact is that removal of activity from stainless steel is difficult compared with mild steel presumably because the oxide film into which the activity diffused is much more adherent.

The Chemistry of Helium Circuits

The choice of an inert coolant for the HTR would at first sight seem to eliminate the normal concern regarding corrosion of the core assembly. Unfortunately, (for two reasons), chemical effects cannot be neglected. Firstly, at initial commissioning and after the loading of new fuel, some absorbed gases will be introduced. Secondly, if the HTR is employed with a steam cycle, some inleakage of steam must be expected and, if oil is used as a lubricant in the circuit, some hydrocarbon contamination will arise. Thus both for the purposes of design and optimising operational procedures, a clear understanding of the chemical reactions of trace impurities in helium is required. In CO_2 cooled reactors, the moderate temperatures lead to a situation in which radiolytic reactions assume precedence over those with thermally controlled kinetics and in-pile experiments are essential to define the design criteria. This situation needed investigation for the HTR.

Dragon provides an ideal environment for conducting experiments on the injection of impurities into the primary circuit. The normal impurity levels are extremely low as shown in Table 2.

The achievement of these levels has been aided by a number of design features (such as a steam pressure below that of the primary circuit) some of which are not possible in a commercial plant where levels would be expected to be higher. The only serious perturbation on the above picture, arises from waterside corrosion on the boilers. When serious boiler corrosion was occurring, the primary circuit hydrogen levels rose, at times up to 40 vpm, but it is hoped that the new boilers and water treatment methods will stabilise the hydrogen below 0.5 vpm.

A large number of experiments have been carried out¹² using both "single shot" and continuous injections. A typical result from a slow water injection is shown in Fig. 8. Space does not permit a detailed analysis here, but the following general conclusions can be drawn.

(a) For Water Injections

- (i) The reactions are simple first order with respect to water in agreement with laboratory experiments.
- (ii) The "water shift" reaction $\text{H}_2\text{O} + \text{CO} \rightleftharpoons \text{H}_2 + \text{CO}_2$ can be neglected under Dragon conditions.
- (iii) Carbon deposition reactions can be neglected (even though $\frac{C_{\text{H}_2}}{C_{\text{H}_2\text{O}}} > 10$) because of the long induction period before metal surfaces become catalytically active ($\sim 10^4$ hours at 100 vpm CO).
- (iv) No oxygen pick-up is observed in the gaseous constituents.
- (v) Mass balances show that only about 65% of the water reacts quickly. The rest is absorbed and presumably released slowly over a long period.

(b) For Gaseous Injections (H_2 , CO, CO_2 , CH_4)

- (i) Oxygen pick-up is observed. The source of oxygen is unconfirmed but assumed to be metal oxides. This effect is more marked with CO than H_2 .

Table 1. Primary Circuit Activity Concentrations (end of Core 7 Charge III)

Isotope	Method of Measurement	Activity Concentration pCi/cm ³
All gases	Ion chamber	13,500
Xe-133	Y-spec. sample	950
Xe-135	Y-spec. sample	4,700
Kr-85m	Y-spec. sample	2,900
Kr-87	Y-spec. sample	4,200
I-131	"Dracule"	19
Cs-137	"Dracule"	Data collected not yet analysed
Ag-111	"Dracule"	

Table 2. Typical Primary Circuit Impurity Levels

Impurity	P.C. Level vpm	Limit of Detection vpm
H ₂ O	<0.05	0.05
H ₂	0.3	0.05
CO	0.16	0.08
CH ₄	<0.04	0.04
O ₂	<0.02	0.02

Table 3. Core Water/Graphite Reaction Rate Constants

Charge	Core	Power MW	R _C h ⁻¹
I	4	20	0.35
II	1	20	1.02
II	2	18	0.90
III	4(a)	15	5.3
III	4(a)	20	10.3
III	6	20	4.0
III	7	20	2.2

(c) Hydrocarbon Injections

- (i) Cracking proceeds rapidly through decreasing molecular weight hydrocarbons to hydrogen and methane. The longer term reactions are those associated with these two gases. Oxygen pick-up occurs.

At this stage in the development of the HTR, the most important conclusion is (a) (i) because it confirms that out-of-pile experiments can give valid design data.

The rate constant for the first order reaction with water is of great significance not only because of its implications on core corrosion but because it is the vital parameter in safety studies on the effects of boiler failures producing explosive water gas. The reaction rate rises so sharply with temperature that a few small areas of high surface temperature graphite can completely outweigh the rest of the core. This is shown dramatically in Table 3.

It can be seen that there has been a general trend towards increasing rate constants but Charge II Core 4(a) had an outstandingly high value. This was due to the introduction of some centrally cooled elements which had a small area of graphite surface at about 1200°C. When small areas of the core can exert such a controlling influence it will be appreciated that theoretical predictions of rate constant are difficult but some success is being achieved. The calculated value for Charge III Core 4(a) is 10.5 h^{-1} (cf. 10.3 experimental) but such agreement is admittedly fortuitous.

A regular series of injection experiments is carried out for successive cores in Dragon. These, as can be seen, from the above, yield overall core figures difficult to interpret in terms of basic data but of direct relevance in confirming the continued applicability of the reactor safety clearance. They are best described as "effective temperature" measurements (weighted by surface area and an Arrhenius temperature relationship).

A new series of experiments is about to commence using special fuel elements in core position 0/0. Arrangements have been made to

reverse the normal purge flow on this element, so that wet helium can be passed over the fuel boxes. These boxes can then be removed for detailed examination to determine weight loss, depth of penetration of corrosion, the effect of irradiation on rate constant by modification of the graphite structure, etc.

Special Instruments for HTR's

The rapid impurity reactions and their selective attack upon limited core areas requires the maintenance of exceptionally low levels of impurity. The first operational requirement is a reliable set of analytical instruments capable of operating down to levels at least an order of magnitude less than those normally of interest.

The instrument selected for water measurement at Dragon was the Goldsmith-Cox electrolytic hygrometer developed for use at high pressures. This instrument is simple and reliable, is capable of being made absolute and improves in sensitivity as the pressure is increased (in terms of vpm levels since it basically measures absolute water pressures and collects more efficiently at low gas velocities)¹³. The ultimate sensitivity is determined by leakage currents arising from contaminants and imperfections in the phosphoric acid/glycerine film. Although capable of detecting 0.003 vpm H₂O in 20 ata helium in the laboratory, plant experience suggests that 0.05 vpm is a practical figure for operational use. Experiments are at present in progress to explore the pulsed use of these instruments. Higher currents will be obtained by using interrupted electrolysis and it is hoped that a further decade of sensitivity will be obtained; that is a useful limit of 0.002 vpm at a circuit pressure of 60 ata. The development of a process-type indicating and supply system should not present any difficulty.

The above instrument has also been successfully proved as a hydrogen monitor by preceding the hygrometer with a small copper oxide furnace at 400°C. Sensitivities are identical with those for water.

The other impurities of interest (CO , CO_2 , O_2 , CH_4 , N_2 and hydrocarbons) are measured using a differential concentration gas chromatograph¹⁴. In this instrument impurities are absorbed on liquid nitrogen cooled traps and then released through a molecular sieve to a katharometer. This construction at once achieves concentration factors of more than 100 and eliminates the leaks and stagnant pockets associated with sampling valves (Figs. 9 and 10). Three types of chromatograph have been built. One uses a nitrogen carrier for determining hydrogen and helium in steam circuit padding gas, the second a helium carrier for use in any helium circuit application. The third type is a portable multipurpose instrument designed for direct attachment to the plant where this was preferable to taking samples to the laboratory instruments. The minimum levels detectable with confidence by these machines are shown in Table 4.

Table 4. Minimum Impurity Levels Detectable by Differential Concentration Chromatographs (with Katharometer Detector)

Impurity	Limit of Detection
A	0.02 vpm in 20 ata helium
O_2	0.02 vpm in 20 ata helium
N_2	0.04 vpm in 20 ata helium
CH_4	0.04 vpm in 20 ata helium
CO	0.08 vpm in 20 ata helium
CO_2	0.02 vpm in 20 ata helium

Such sensitivity is remarkable considering the use of a katharometer and experiments are now starting with the use of a helium ionisation detector. This system has been used mainly with an argon carrier but should have greater sensitivity with high purity helium as it exploits the high ionisation and excitation potentials of the rare gases. Improvements of one and possibly two decades of sensitivity are hoped for.

THE MAINTENANCE OF HELIUM CIRCUITS

It is natural for operators considering the maintenance of an HTR primary circuit to be concerned by the absence of metallic cladding of the fuel and possible consequent activity hazards. Fuel loaded into an HTR will be supplied to a release specification which is not zero as with other reactors at present in use. The hard lesson of experience in present power reactors is, however, that even in the absence of some failed fuel (and some failed fuel is the usual situation), very significant radiation hazards develop from activated crud or corrosion products. The minimal corrosion in the HTR eliminates this problem and one is concerned only with the fuel integrity. Many utilities are rightly firm in their demands that boiler and blower inspection should be possible throughout the life of the station. This requirement resolves itself into two main areas. The problems of ingested activity centre around I-131 and here Dragon experience is very relevant. On the other hand long-lived isotopes such as Cs-137 give rise to a direct radiation hazard but Dragon experience cannot be taken directly because of the relatively short history.

Between November, 1969 and March, 1970, Dragon was shutdown for a complete replacement of the boilers and steam loops. As a test case, very careful measurements were taken of radiation levels, body burdens, and contamination levels around the plant. Shortly before shutdown, the primary circuit activity level was unusually high and estimates indicate about 12 Ci of I-131 external to the fuel particles. At the time of opening the circuit this would have decayed to about 1.5 Ci. As a matter of interest this amount of I-131 is sufficient to produce one maximum permissible concentration for inhalation (mpc) in about $2 \times 10^8 \text{ m}^3$ of air or 2.5×10^4 containment building volumes. Since under maintenance conditions with open air-locks, one air change occurs in about 10 hours it can be seen that a fractional release rate of about 4.5×10^{-6} per hour of the "free" iodine would give rise to 1 mpc in the containment building. The highest general airborne level was about 1 mpc - an astonishingly small release.

Prior to opening the circuit, the helium atmosphere was exchanged with dry nitrogen to minimise convection effects at the open flanges (about 80 cm in diameter). Apart from this no attempt was made at containment during removal of the boilers and circulators. After removal each flange was closed with a simple light alloy cover with an inflatable seal.

The circulators were drawn back on rail trolleys¹⁷ and sheeted with PVC as soon as clearance was available (Fig. 11). The radiation levels on the impellers were generally about 200 mr/h at 1 ft. Air masks were worn as a precaution during this phase although not essential. The highest airborne I-131 level in the actual opening was 45 mpc. One man who worked for about an hour around the open hole without an air mask was found subsequently to have a thyroid burden of 170 μCi compared with the ICRP permitted level of 700 μCi for continuous working.

The boilers were withdrawn vertically into PVC sleeves (Fig. 12) and gave radiation levels of about 50 mr/h β/γ on contact with the plastic sheet. Airborne release during this phase was negligible and air masks were not worn. In order to minimise release at the openings a small suction was maintained on the thermocouple branches lower down the ducts. This is a useful technique worthy of application elsewhere although it probably increases the oxygen contamination of the nitrogen blanket gas. During the whole process of changing 6 boilers and circulators, which lasted about 4 weeks, no person was exposed to internal or external radiation to a level imposing any individual restrictions.

Contamination control proved exceptionally straightforward. A local control area was set up immediately adjacent to the primary circuit openings and a coverall and overshoe routine established. By this means the spread of contamination to the main containment building was limited to an extent that "blue" area conditions applied ($<10^{-4} \mu\text{Ci}/\text{cm}^2$) and no protective clothing was required. In the great majority of the containment building public access would have been permissible.

Summarising, a major maintenance operation has been carried out on Dragon with primary circuit conditions at least as severe as those expected on a power HTR. No significant difficulties were encountered, either in respect of the mechanical operations or the control of radiation. The core remained full of irradiated fuel throughout this process and the experience suggests that maintenance may well be quicker and easier than on other reactor types.

During the period following shutdown, a boiler tube failure released some water into the primary circuit and the water level reached 5.5%. In addition to this when the circuit was nitrogen blanketed with open flanges, some air inleakage occurred and at one stage the oxygen concentration reached 1%. After reclosing, the circuit was evacuated and filled with clean helium. By this means, the "free" contaminants were reduced to a low level which was further improved by the operation of the clean-up plants during the latter stages of the shut down. However, it was expected that considerable absorption of impurity on the core and reflector graphite would take place. These impurities would not be removable until driven off by the circuit heating as the approach to power took place. This process can be clearly seen in Fig. 13. Large temperature steps are accompanied by sharp rises in impurity level. In order to minimise core corrosion each rise is reduced by the clean-up plants before the next step is applied.

A further complicating factor was the injection of hydrogen produced from forming the magnetite film on the new boilers. Past experience has shown that hydrogen diffuses rapidly through the tubes from its point of formation. For this reason the hydrogen level stays above the equilibrium $H_2/H_2O = 8/1$ which would be expected at full power. This equilibrium is shown as a dotted line in Fig. 13 from which it appears that the hydrogen not produced from water is falling slowly, e.g., from 3.0 vpm on 28th March to 0.8 vpm on 1st April. The carbon monoxide level is seen to follow the hydrogen level quite closely and again to follow about an 8/1 relation with water.

The most significant demonstration from this process is the small uptake of water and oxygen by the core during open-circuit maintenance in spite of very high contaminant levels.

ACKNOWLEDGMENTS

The experience described in this review is culled from 5 years efforts by Dragon Operations and Project Staff too numerous to mention by name. I thank both them and the Chief Executive, Dragon Project for the privilege of reporting such a convincing demonstration of the viability of the HTR concept.

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DRAGON POWER HISTORY

1. Dragon Operating History.

MASS SPECTROMETER READINGS DURING CHARGE III
CORES 5-7

2. Inner Containment Helium Concentrations.

3. Helium Usage.

4. Compressor Diaphragm Life.

5. Schematic Diagram of Dracule.

6. I-131 Profile in Dracule.

7. Sectioning a Primary Heat Exchanger.

8. Results of a Water Injection Experiment.

9. Flow Sheet of Differential Concentration Gas Chromatograph.

10. Process Model Chromatograph.

11. Circulator on Removal Trolley.

12. Primary Heat Exchanger (Boiler) after Removal.

13. Impurity Levels during Restart of the Reactor.

DISCUSSION

J. Kemper: What future plans have been made for Dragon?

B. G. Chapman: As you can see, we have lost considerable time as a result of boiler tube failures, but we believe this problem is now behind us. What we want now is two or three years of steady operation to concentrate on testing power reactor fuel pins, on further chemical corrosion work, and fission product transport and deposition studies.

Bender: Helium leakage during early period of operation — was it a problem?

B. G. Chapman: We had no serious problem. All circuit components were stringently tested at the Contractor's works by our own specialist teams. The residual problems of testing site work are not difficult if one vital precaution is observed. The helium background in the plant area must be kept very low so that leak location is facilitated. Because concrete has a long "memory" for helium, all gas (either helium or 10% helium in nitrogen) used for pressure and leak testing, must be vented after test outside the plant buildings. If "heliarc" welding is used local extract systems should be provided.

It is interesting to note that during a recent shutdown at Dragon a small quantity of helium was released (from welding) and our containment building levels are still well above normal and will take several months to clear.

A. P. Fraas: To what degree would your maintenance activities have been handicapped if the level of radioactivity in the gas circuit had been increased by a factor of 100?

B. G. Chapman: Activities would have been hampered but not rendered impossible. The airborne activity would have required airhood protection around open holes, but the containment building levels would not have been unacceptable. More urgency would have existed for fitting temporary seals on openings. Radioactive contamination was found to be very firmly adherent and control would be not seriously more difficult. Direct radiation would require some small amount of temporary shielding or fairly tight working time limitation.

R. Huddle: I would like to comment on W. Chapman's remark regarding failed fuel. We have a very large amount of experimental fuel in Dragon which is designed to fail - to obtain failure data. The failed fuel which Chapman referred to was when the purge on one of these elements ceased to be effective. The important point is that it shows that we do not necessarily need perfect fuel for HTR's.

OPERATING EXPERIENCE WITH THE AVR
EXPERIMENTAL POWER STATION

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ABSTRACT

This report describes the operating performance of the AVR experimental power station during its commissioning program as well as power operations following customer acceptance of the plant in May 1969. The plant outages, which have occurred since the power commissioning program began on December 18, 1967, are enumerated and evaluated according to their nature and duration. The overall plant availability, to March 31, 1970, has been gratifyingly high and amply demonstrates the feasibility of the pebble bed reactor concept.

INTRODUCTION

The AVR experimental station employs a high temperature, helium-cooled reactor and is the first power plant to use spherical fuel elements. The core consists of a randomly packed pebble bed containing approximately 100,000 spheres, each having a diameter of 6 cm. The contract for the construction of this demonstration power plant was signed on August 13, 1959 by the Arbeitsgemeinschaft Versuchs-Reaktor GmbH (AVR) and the Brown Boveri/Krupp Reaktorbau GmbH (BBK). Actual construction of the plant began nearly two years later, having been delayed by differences of opinion concerning the technical design of the reactor foundation and by the lack of regulatory laws in the Federal Republic of Germany governing the construction and operation of nuclear power stations. The AVR station is shown in Fig. 1.

First criticality was achieved on August 26, 1966 and the first production of electricity began on December 18, 1967 following a comprehensive program of zero power experiments. The total electrical energy generated by March 31, 1970 is 138.1×10^6 kWh. The fuel elements have

attained an average burnup of 43.3% fima, corresponding to 7.0% fima. The plant availability factor, taken as the ratio of generator operation time to total calendar time, has been quite good. The overall plant availability factor for the 27 month period of plant operation is 64.6%. The plant availability achieved for selected periods of operation are:

51.4% during the year 1968,

92.0% during the winter months from October 1968 to March 1969,

71.6% during the year 1969, and

95.4% during the winter months from October 1969 to March 1970.

Because of the good operating experience obtained, the power station was turned over to the AVR organization by BBK in May 1969. Prior to the acceptance of the plant by the customer, a final thorough inspection of the various plant components was made by the constructor firm. The inspection and maintenance program, conducted from March 1, 1969 to April 15, 1969, revealed that the degree of radioactive contamination of primary circuit components was smaller than had been expected, for example, disassembly of the fuel handling facility was possible without special shielding precautions. The activity level of the primary coolant gas has been consistently low, about 300 curies, throughout reactor operations to date. This indicates good fuel element performance with no deterioration of their fission product retention capability occurring due to fast neutron exposure and burnup.

The operational experience acquired with the AVR Experimental Station over the past two years verifies the validity of its basic design and establishes the pebble bed concept as a viable alternative within the HTR reactor line. There has been no evidence of fuel spheres being unduly damaged by the severe environmental conditions of the core or by the mechanical loads imposed upon them during normal fuel handling operations. The basic feasibility of the pebble bed reactor concept has been amply demonstrated and the overall performance of the power station has been such as to justify the construction of a larger, prototype pebble bed power station. Present plans call for the construction of a 300 Mwe high temperature, pebble bed power station to begin late this year.

Fig. 1. The AVR Power Station in Jülich.

Fig. 2. View of Reactor Building.

DESCRIPTION OF THE AVR POWER STATION

A brief description of the AVR power station is provided here to clarify the discussion which follows. More detailed information is readily available in the open literature. Figure 2 shows a section view of the reactor building. The core is filled with approximately 100,000 spherical fuel, graphite and boron elements. The AVR fuel element consists of an unfueled graphite shell filled with coated uranium-thorium dicarbide particles which are uniformly dispersed in an inner graphite matrix. Each fuel element contains 1 g of 92% enriched uranium and 5 g of thorium. The core is doubly contained in two steel pressure vessels, with pure helium maintained at a slight overpressure in the volume between the two vessels to prevent possible outward leakage of the contaminated coolant gas.

The coolant gas enters the core through radial slots in the bottom reflector and streams upward through the core. Two helium blowers circulate the coolant gas through a carbon brick bridge, located above the surface of the pebble bed, to the steam generator. The carbon brick bridge serves to shield the steam generator from fast neutrons. This is important because the steam generator is not replaceable due to the integrated construction which has been used. The steam generator is a forced-flow Benson system with four parallel sections, each having its own regulatory and control devices.

The fuel handling system is comprised of gas locks, mechanical switches, and transport tubes through which the fuel spheres are charged into the core by means of helium pressure. During reactor operation, the fuel spheres are recycled continuously from the bottom of the core back into the top in order to provide fuel mixing and attain uniform burnup and a flat power distribution. Spheres are removed from the core by the force of gravity and the pneumatic energy required to elevate the spheres is provided by the pressure differential generated by the main helium blowers. Thus, the fuel handling system is simple and rugged and possesses a minimum number of components with moving mechanical parts.

AVR PLANT OPERATING EXPERIENCE

Power Program

A summary of AVR power generation achievements since December 1967 is presented in Tables 1 and 2. As of March 31, 1970, a total of 12,912 hours of generator on-line time had been achieved and the total electrical energy produced amounted to 138,075 Mwh. The fuel elements have attained an average burnup of 43.32% fima, corresponding to 7.0% fima. At the present time, the core and fuel handling circuits contain 47,729 fuel spheres. This number is gradually increasing as fresh fuel elements are loaded and dummy graphite spheres are discharged. The maximum power level reached thus far is 50 Mwth, or 15.6 Mwe. This was achieved by increasing the helium blower speed from 4000 to 4400 rpm and raising the coolant gas pressure from 10 to 11 atm. The insufficient shutdown worth of the rods had previously prevented operation at the nominal design level, 15 Mwe.

The power history of the AVR station is depicted in Fig. 3. During this 27 month period, some 60 interruptions in power generation occurred due to reactor scrams and scheduled plant shutdowns. In Fig. 3, the scheduled plant shutdowns are identified by the capital letters A through Q, and the unscheduled shutdowns initiated by the automatic safety circuits are identified by numbers. In the case of an actual commercial power station, the scheduled plant shutdowns would have allowed sufficient time to bring reserve stations on line whereas the automatic scrams would have resulted in a temporary reduction in the total power available to the grid. This distinction, however, is not applicable to an experimental power station, particularly one such as the AVR undergoing its commissioning tests. Here, it serves merely to differentiate between outages occurring unexpectedly during routine reactor operation and those which were clearly foreseeable.

Scheduled Plant Shutdowns.--The scheduled plant shutdowns shown in Fig. 3 are grouped according to their causes and location in the plant system and listed in Table 3. Portions of the total outage periods are somewhat arbitrarily assigned to specific components and systems even

Table 1. Summary of AVR Operations During the Year 1968

	Jan	Feb	Mar	Apr	May	June	July	Aug	Sept	Oct	Nov	Dec
Calendar hours	744	696	744	720	744	720	744	744	720	744	720	744
Generator hours	349	285	725	374	-	259	427	180	-	514	680	708
Cumulative generator hours	646	931	1656	2030	2030	2289	2715	2895	2895	3409	4089	4797
Core thermal operation, h	350	293	741	374	-	321	454	193	-	550	702	722
Gross electrical generation, Mwh	1820	2841	7269	4087	-	2336	4061	1586	-	4682	6307	6558
Plant requirements, Mwh	726	771	1023	549	-	383	648	254	-	840	1120	1185
Net electrical production, Mwh	1093	2070	6246	3538	-	1954	3413	1332	-	3842	5187	5373
Thermal energy produced, Mwh	7972	9299	23376	13056	-	9195	13973	5907	-	17434	22524	23477
Cum. thermal energy (since 9-19-67) (Mwh)	853	1241	2215	2759	2759	3142	3724	3970	3970	4697	5635	6613
Cum. gross electrical (since 12-17-67) (Mwh)	3086	5965	13197	17284	17284	19620	23681	25267	25267	29949	36257	42815
Cum. electrical to power grid, Mwh	2352	4792	10951	14380	14380	16381	19771	21087	21087	24885	29994	35277
Max. electrical power level, Mw	5.9	10.1	10.4	14.2	-	9.8	10.0	9.8	-	9.5	9.4	9.3
Max. thermal power level, Mw	23.2	32.4	33.9	45.4	-	33.2	33.8	31.9	-	34.1	32.9	33.0
Total charged fuel elements	23139	25589	25589	25589	25589	25736	25736	26310	26429	27150	27745	28150
Fifa, %	3.87	5.48	9.1	11.32	11.32	12.83	15.21	15.86	15.79	18.19	21.35	24.70
Fima, %	0.63	0.89	1.47	1.83	1.83	2.08	2.46	2.57	2.56	2.95	3.46	4.00
Plant electrical availability factor, %	47.04	40.95	97.45	51.94	-	35.97	57.39	24.19	-	69.09	94.44	95.16

Table 2. Summary of AVR Operations During the Year 1969-70

	Jan 69	Feb	Mar	Apr	May	June	July	Aug	Sept	Oct	Nov	Dec	Jan 70	Feb	Mar
Calendar hours	744	672	744	720	744	720	744	744	720	744	720	744	744	672	744
Generator hours	687.5	629	-	220	564	626	710	60.5	595	738	720	727	602	671	565
Cumulative generator hours	5485	6114	6114	6334	6898	7524	8234	8294	8889	9627	10347	11074	11676	12347	12912
Core thermal operation, h	705.5	650	19	309	585.5	645	711	208	630	743.5	720	743.5	611	672	571
Gross electrical generation, Mwh	6694	6567	-	2596	6855	7590	8319	746	7098	8388	8293	10122	7997	7539	6456
Plant requirements, Mwh	1125	1039	-	352	913	986	1117	78	975	1212	1192	1320	1008	1133	965
Net electrical production, Mwh	5569	5528	-	2244	5942	6604	7202	667	6123	7176	7101	8802	6989	6406	5491
Thermal energy produced, Mwh	22963	21867	219	9101	22075	24634	27340	3179	24201	28053	26389	32781	25879	24426	21620
Cum. thermal energy (since 9-19-67) (Mwh)	7570	8481	8490	8869	9789	10816	11955	12087	13096	14265	15364	16730	17808	18826	19726
Cum. gross electrical (since 12-17-67) (Mwh)	49509	56077	56077	58672	65527	73117	81437	82182	89281	97668	105962	116083	124080	131619	138075
Cum. electrical to power grid, Mwh	40693	46058	46058	48246	54038	60475	67490	68120	74080	81057	87967	96554	103344	109547	114867
Max. electrical power level, Mw	11.8	12.84	-	12.65	12.90	13.17	13.17	12.9	12.7	12.43	12.38	15.6	13.28	11.13	11.43
Max. thermal power level, Mw	38.6	40.6	11.4	39.2	39.73	41.47	41.44	41.08	40.78	40.60	38.54	50.76	42.36	36.35	37.86
Total charged fuel elements	32597	34095	34155	34855	35895	36917	38015	38065	39115	40925	42615	44415	45435	45955	47875
Fifa, %	24.42	26.15	26.13	26.76	28.67	30.80	33.06	33.38	35.20	36.64	37.90	39.60	41.21	43.07	43.32
Fima, %	3.96	4.24	4.23	4.34	4.64	4.99	5.36	5.41	5.70	5.94	6.14	6.42	6.68	6.98	7.02
Plant electrical availability factor, %	92.40	93.60	-	30.56	75.80	86.94	95.43	8.13	82.63	99.19	100	97.71	80.91	99.85	75.94

though the maintenance activities were closely interrelated and usually performed concurrently. While the apportionment of the outage periods to particular components is admittedly imprecise and depends upon an arbitrary exercise of personal judgment, a qualitatively valid picture of the location and relative importance of the various malfunctions emerges. In assessing this information, it is important to note that 85% of the total plant downtime occurred during the "shakedown" phase of plant run-in operations prior to customer acceptance of the experimental power station. A full description of the nature of each scheduled shutdown is provided in the Appendix.

Unscheduled Plant Shutdowns.--The unscheduled shutdowns caused by the action of automatic safety circuits during reactor operation are numbered and shown in Fig. 3. The loss of components such as diaphragm compressors, main feedwater pump, etc., which are necessary for continued reactor operation are also included in this category. The 43 incidents of this nature which have occurred are grouped in Table 4 according to their causes and location in the plant system. The most frequent and annoying cause of power outages is diaphragm ruptures of the large, water-driven, helium compressors. Although the plant design provides for a standby reserve unit, the compressors are unfortunately located in a radiation area so that maintenance cannot be carried out while the reactor is at power.

This situation was greatly alleviated by the installation of a piston type Sulzer compressor in May 1969. This compressor, located in an area which is serviceable during reactor operation, has exhibited superior operating performance. A total of 3500 hours of operation was attained before the first maintenance and repair were required. The Sulzer compressor has now been in operation for a total of 7800 service hours, with only 100 hours of downtime. It is expected that the current maintenance interval of 3000 hours can be considerably lengthened as operating experience is acquired. The introduction of this machine to the AVR gas purification plant system has largely eliminated one of the most frequent causes of plant outages.

Table 3. Breakdown of Scheduled AVR Plant Shutdowns from Dec. 18, 1967 to March 31, 1970

(Generator Off-Time, h)																		Totals
Outage Period	A	B	C	D	E	F	G	H	I	J	K	L	M	N	O	P	Q	
Fuel Handling Facility:																		
Scrap Separator																60	130	
Severlizer			145															
Singulizer			145															
Selector wheel					530						79							
Dosierrad																		
Counter coils					420													1509
Gas Purification Plant:																		
RA-4 tube rupture				302														
Diaphragm compressors					51		55											
RA-4 installation					400													
Water ingress RA-4														300				
Oil ingress to RA-4														200				1308
Secondary Circuit:																		
Turbine balance																		
Feedwater pump									72									
Pressure regulator										27								
Turbine inspection						1		10										110
Other:																		
Blower lubrication system																	48	
Dynamics experiments													15	77		2	19	
Inspection, maintenance		370	577				1190						100			60		
Rod drive motor			120															
Commissioning	72																	
Fuel loading		370																
Fuel recycling			450															
Operator error														111				3681
Generator Off-time (h)	72	740	1537	302	1401	1	1245	10	72	27	79	15	177	611	2	139	178	6608

Table 4. Breakdown of Unscheduled AVR Plant Shutdowns
(Dec. 1967 to April 1970)

Designation of Malfunction	Cause of Power Outage (Nature of Malfunction)	Generator Off-time (h)
<u>Fuel Handling Facility:</u>		
Difficulties with the fuel handling facility were either repaired on-line or resulted in a scheduled shutdown listed in Table 3.		
<u>Gas Purification Facility:</u>		
3,6,12,16,17,21,25 and 33	Diaphragm compressor failure	255 h 17 m
<u>Secondary Circuit:</u>		
1,27	Loss of main feedwater pump	27 h 10 m
2	Oil circuit for turbine	13 m
18,30,36,37	Live steam valve malfunctions	109 h 46 m
		137 h 09 m
<u>Electrical Circuits:</u>		
7,8,14,26,34,35	Circuit breaker overload	82 h 49 m
19,20,23	Grid voltage fluctuations	11 h 59 m
28	Defective signal amplifier (in L)	0
		94 h 48 m
<u>Others:</u>		
4,5,10,15,29,32,41,43	Operator error	9 h 45 m
11,24,39	Scrams of unknown or external origin	17 h 22 m
9,42	Loss of blowers	54 m
22,38	Brush sparking of turboalternator	3 h 33 m
13,40	Defective rod drive system	56 h 3 m
		87 h 37 m

Fig. 3. AVR Power Generation Record.

Fig. 4. AVR Performance Diagram for 1968/69.

AVR Plant Availability

The availability of the AVR power station, calculated as the ratio of generator on-line time to total calendar time, has been exceptional for an experimental power station undergoing its commissioning tests. As can be seen in Fig. 4, the plant performance has improved steadily since power operation began in late 1967. In 1968, the overall plant availability was 51.4%, in spite of extended plant shutdowns for fuel loading and maintenance of the gas purification and fuel handling facilities. The availability factor for the year 1969 was 71.6%. During the winter months from October 1968 to March 1969, the overall plant availability was 92%, increasing to 95.4% for the same time period in 1969-70. If one takes into account all power operations to date, including both the commissioning and run-in phases of operation prior to customer acceptance of the plant, the overall availability amounts to 64.6%.

AVR Reactor Core

Fuel Behavior.---Although the average fuel burnup has reached 7% fima, the fission product retention of the fuel has remained largely unchanged and good. Throughout the two years of power operation, the activity level of the coolant gas has remained essentially constant at 300 curies. This activity arises mainly from uncoated uranium present in the fuel elements. The R/B ratio for ^{133}Xe is one tenth of the specified value of 5×10^{-4} . A portion of the first charge fuel has already experienced a burnup of 12% fima, considerably more than the fuel specification value of 9% fima. Nevertheless, it is planned to retain these highly burned fuel elements in the core for some time longer and to deliberately recharge them to the highest flux zone during recycling operations. The purpose of this action, which will probably lead to a partial destruction of the fuel particle coatings, is to demonstrate the operability of a pebble bed reactor even when some of the fuel elements become defective.

Primary Circuit Impurities.---The concentrations of gaseous impurities in the primary circuit, arising from the outgassing of core structural material during power operation, have declined steadily due to the cleansing action of the gas purification plant. The present levels of these chemical contaminants are: CO - 30 vpm, H_2 - 9 vpm, CH_4 - 2 vpm,

CO₂ - 5 vpm, and H₂O - 0.1 vpm. The presence of these impurities in the primary circuit has not resulted in any appreciable corrosive damage to the fuel elements.

Refueling Program.--Fuel loading and recycling is carried out during power operation at a rate set mainly by core reactivity considerations. The German licensing authorities require that the core must not be loaded to a reactivity which exceeds the worth of the four shutdown rods less 1/2 Nile, as measured with respect to the reference shutdown temperature of 105°C. The complete AVR fuel element circuit presently contains 107,381 fuel, boron, and graphite spheres. Of these, 13,200 spheres are located in the sphere discharge tube at the base of the core. Table 5 lists the present composition of the reactor core and associated fuel circuits. Of the 47,729 fuel spheres in the AVR circuit, some 30,000 are the first charge U.S.-manufactured, machined fuel spheres, 4390 are the "wallpaper" type, and the rest are spheres manufactured by new molding techniques developed by the German firm Nukem.

Table 5. AVR Core Composition, March 31, 1970

Sphere Type	Complete Fuel Circuit	Core	Discharge Tube
Fuel	47729	42840	4889
Graphite	57354	49363	7991
Boron	2298	1978	320
Total	107381	94181	13200

The fuel loading rate is chosen on the basis of theoretical predictions obtained from two-dimensional, multiple-zone burnup calculations which incorporate the known loading and recycling program as well as the flow behavior of the spheres within the pebble bed. Deviations from the predicted behavior are manifested in the form of changing gas exit temperatures from the core. When this occurs, the refueling program is altered to re-attain the desired operating conditions. However, a time

Fig. 5. Sphere Flow Characteristics in the AVR Pebble Bed Core.

Fig. 6. Example for a Representation of AVR Core in 2-D Erebus Burnup Calculation.

delay of 4 to 6 weeks exists before the altered refueling and recycling program takes effect. There is therefore a high incentive to obtain quantitative accuracy in the burnup calculations so that the optimal fuel loading program can be selected.

Burnup calculations for a pebble bed reactor are complicated by the fact that the zonal nuclide concentrations in the core vary both with fuel burnup and fuel movement. Figure 5 shows the flow pattern of spheres as they traverse the core. The surface profile lines clearly show that the sphere velocity depends strongly upon its radial position in the core. The power distribution calculations must adequately account for the continuously changing core configuration as well as the long term fission product accumulation. Figure 6 shows how the movement of fuel spheres through the core is simulated in the EREBUS burnup program by multiple zone core calculations at discrete time steps.

Core Physics Measurements

Rod Drop Experiment.--Several rod drop experiments have been performed to determine the reactivity worth of the four rod bank. The four rods are scrambled during reactor power operation and the resulting flux-time trace is recorded. An acoustical record of the noises generated by the rack and pinion gears of each rod is used to determine the depth of rod insertion as a function of time. The flux-time trace is analysed by an inverse kinetics program developed by Mr. K. J. Kalker of the KFA Nuclear Research Center at Jülich. Although the theoretical basis of the experiment is not fully understood, the experimental results agree well with estimates of the total rod worth obtained by other methods. The rod drop experiment has been adopted as a standard means for periodically investigating changes in rod worth due to fuel burnup and altered core configuration.

Simulated Rod Failure.--The marginal shutdown worth of the AVR rod bank makes it important to know the consequences of a possible single rod failure. For this reason, experiments have been performed to measure the nuclear behavior of the hot core when held subcritical by only three of the four rods. In these experiments, the reactor core was allowed to

cool by natural convection for a period sufficiently long to establish the cooling down rate. The blowers were then used to forcefully cool the core until it again reached criticality by virtue of the negative temperature coefficient of reactivity. This was done to expedite the cooling process which would otherwise require several weeks. It was found that the reactor power levels out to about 30 kw, after undergoing oscillations to 50 kw, following its return to critical. The experiment demonstrates that at least several weeks would be available to provide additional shutdown reactivity if one rod fails to operate during a scram at full reactor power.

Temperature Coefficient.--The temperature coefficient of reactivity has been determined by means of period measurements of a given core configuration made at two different temperatures. At the reference shutdown temperature of 105°C, the temperature coefficient of reactivity was found to be -18.9 mN/°C. This value, containing an estimated uncertainty of 10% due to errors in measuring the temperature, is the average of three separate measurements and agrees fairly well with calculated results. The experimental procedure employed was to establish a slightly supercritical rod position at a core temperature of 103°C and to then measure the reactor periods of this core configuration for core temperatures of 103°C, 110°C, and 114°C. The core temperature was varied by adjusting the temperature of the feedwater to the steam generator.

Tritium Measurements in the AVR Reactor.--The AVR coolant gas contains very little tritium during normal power operation, on the order of about 0.5 curies. The primary sources of tritium in the AVR are the (n,α) reaction with Li-6, which is present as an impurity in the graphite and carbon brick structural material, and directly as a fission product. Tritium has also been detected in the AVR secondary circuit in concentrations of about 2.5 microcuries per liter of feedwater, corresponding to an overall activity of about 0.14 curies in the steam circuit. The origin of the tritium found in the secondary circuit is not yet clear. It is suspected that at least a portion of the secondary circuit tritium is produced by neutron activation of nitrogen-14, an isotope present in the feedwater from the ammonia and hydrazine used to prepare and process the feedwater.

The main portion of the tritium found in the secondary circuit, however, must arise either from another more productive activation process or from diffusion of the primary circuit tritium through the steel tubing of the steam generator. Several experiments have been performed to ascertain which of these represents the primary mechanism. These experiments are based on the determination of the rate of tritium buildup in the steam circuit. If the tritium in the feedwater arises from neutron activation of some impurity, the time required for its buildup to equilibrium concentration should be relatively long. If, on the other hand, the tritium diffuses from the primary circuit into the secondary circuit, its concentration in the steam circuit should build up in a manner similar to that in the helium circuit.

During a reactor shutdown period, in which one would expect neither activation or diffusion to occur, the entire feedwater was replaced with fresh water containing no tritium. Following the reactor startup, the tritium concentrations in the helium and steam circuits were carefully monitored. It was found that tritium built up in both circuits proportionally, indicating that a diffusion process with a delay time of only a few minutes was taking place. However, in another experiment, some 5 curies of tritium together with several hundred liters of hydrogen were injected into the primary circuit. The tritium and hydrogen concentrations in the steam circuit were then monitored. Astonishingly, no increase in their levels could be detected.

The reason for the negative result of the tritium injection experiment is not clearly understood at the present time. It is therefore planned to repeat this experiment, under more carefully controlled conditions and using more sensitive measuring procedures, sometime in the near future. It is presently believed that only direct diffusion of tritium through the steam generator tubing can account for the concentration observed in the secondary circuit. The yields of other possible reactions are considered to be too small to sufficiently explain the effect.

Ammonia Injection Experiment.--In laboratory tests, ammonia when admixed to the helium coolant gas has been shown to act as a gaseous lubricant, reducing the coefficient of friction between graphite spheres in a pebble bed reactor. In anticipation of possible future requirements in large pebble bed reactors, ammonia was injected into the AVR primary circuit in an experiment conducted on January 23, 1969. The purpose of the experiment was to determine the stability or decomposition rate of NH_3 in a realistic reactor environment of radiation and temperature. The decomposition rate of the ammonia in the primary circuit was determined by observing the growth of the decomposition products, hydrogen and nitrogen. The concentrations of these impurities were measured by gas chromatography at regular intervals following the injection of the ammonia. The decomposition half-life for ammonia was measured to be from 8 to 12 minutes in the AVR core environment.

Dynamics Experiments.--Reactor operating parameters such as the feedwater flow rate, the coolant gas blower speed, and the position of the shutdown rods have been varied stepwise to determine the dynamic response characteristics of the plant. Further tests have been conducted to determine the consequences of the loss of sensitive components such as the main feedwater pump and the helium circulators. In the case of loss of connection to the electrical grid, it was demonstrated that the generator can supply the plants own needs without difficulty. In all the simulated incidents, the automatic safety circuits operated according to plan and functioned reliably to prevent any unsafe reactor condition from arising.

SUMMARY AND CONCLUSIONS

The successful operation of the AVR experimental power station over the past 27 months verifies the validity of its basic design and establishes the pebble bed concept as a viable alternative within the HTGR reactor line. The availability of the plant during this period of operation, which includes the power commissioning program, has been exceptionally good for an experimental power station. Even more gratifying and significant is the fact that the plant availability record has

improved steadily as familiarity and experience with complicated plant systems, such as the gas purification plant and the fuel handling facility, were acquired. Improvements and revisions made during the shake-down phase of power operations have eliminated many weak spots and greatly improved the overall reliability of the power station.

In our opinion, the promising potentiality of pebble bed reactors and the good performance of the AVR power station justifies the planned construction of a 300 Mwe prototype power station based on the pebble bed concept. The prospects for fully automating the fuel element production process, using sphere molding techniques developed by Nukem could greatly reduce fuel element manufacturing costs, a key item in assessing the economic potential of pebble bed reactors. It should be kept in mind that although good performance has been obtained in accelerated capsule tests, adequate fuel element behavior at the required burnup and fast neutron exposure must be demonstrated in reactor experiments. The excellent safety characteristics, good fuel cycle economy and high efficiency of pebble bed power stations combine to make the prospects quite promising that this type of power station will emerge as a serious competitor in the future energy market.

APPENDIX

This appendix provides a detailed description of the scheduled plant shutdowns depicted in Fig. 3, including the date, nature and duration of the various outage periods.

Scheduled Plant Shutdowns

- A. December 19 to 22, 1967 - A prescribed inspection of the turbine bearings was performed as part of the commissioning program. Plant down time, 72 hours.
- B. January 15 to February 15, 1968 - This shutdown period served as part of the commissioning program for the fuel handling facility. During the shutdown period, some 2450 additional fuel elements and 6370 graphite elements were loaded into the core thereby increasing the core reactivity from 8.68 to 10.61 Niles. As is the case for all prolonged plant shutdowns, the outage period was also utilized to carry out repairs, revisions, and improvements on various components. Plant down time, 740 hours.
- C. April 16 to June 19, 1968 - The plant was shut down for a comprehensive inspection and maintenance of plant components and to concurrently carry out a major recycling of the in-core fuel elements under cold reactor conditions. During the reactor shutdown, acceptance tests were performed on secondary circuit components and experiments to simulate the loss of electrical load and loss of the main feed-water pump were conducted. The dynamic response of the plant to these simulated incidents was determined and the automatic safety system was found to perform according to design.

The sphere recycling program was delayed by difficulties which arose with the fuel handling facility. Mechanical components such as the severalizer, singulizer, and sphere locks located in the fuel transport tubes jammed frequently because of the higher coefficient of friction in a dry helium atmosphere. The problems were gradually resolved and by June 12, 1968 some 6120 spheres had been recycled, resulting in a reactivity loss of 560 mN as the central boron zone was pulled deeper into the core. Malfunctioning of the fuel handling

facility became less frequent as familiarity and experience with the system were acquired.

By June 14, 1968, thermal power operation could be resumed and five days later the coolant gas impurities dropped to a level permitting higher gas outlet temperatures from the core. On June 19, 1968, steam conditions allowing turbine operation were attained and electrical power production resumed. Plant down time, 1537 hours.

- D. July 15 to July 27, 1968 - The reactor was shut down in order to inspect the low temperature stages of the RA-4 gas purification plant. Four days earlier large helium losses had been noted after these units had been charged with helium at operating pressure. The inspection revealed that the helium tubing in the low temperature units had ruptured because it had been mounted too rigidly to accommodate the forces of contraction. The low temperature sections of the gas purification plant were dismantled and removed for repair outside the reactor building and the plant resumed operation at 8 Mwe. The forced shutdown period was utilized to install additional thermocouples on the outer pressure vessel lid and to perform acceptance tests on the fuel burnup and identification device. Other small repairs and improvements, such as the installation of microphones along the transport tubes of the fuel handling facility to provide acoustic monitoring, were effected. Plant down time, 302 hours.
- E. August 9 to October 6, 1968 - This plant shutdown, of nearly two months duration, was initiated by a diaphragm rupture in the helium compressor of the gas purification plant. The decision was taken to extend the outage period for two weeks to install the RA-4 gas purification units which had been repaired in the meantime. In parallel with the RA-4 installation, core drying operations were undertaken to remove moisture from the primary circuit which had been admitted as a result of the diaphragm rupture of the water driven compressor MK-2. This was done by circulating the primary gas through a BaO filter for five days and subsequently operating the reactor at a low thermal power level of 1 Mw. By August 25, 1968, the core drying operations and the RA-4 installation work were completed, but resumption of power operation was delayed by a new series

of difficulties with the fuel handling facility.

The recycling of in-core spheres, which had continued during the initial phase of the shutdown period, was halted on August 17, 1968 by weak electronic signals from the sphere detection coils located along the pneumatic transport tubes. These coils detect the passage of a sphere through the transport tubes and provide control signals to the Zuse 32 computer used to direct and record fuel handling operations. Investigation revealed that the weakened signals arose from the altered physical properties of graphite caused by neutron irradiation. The amplification of these signals was improved and the sphere recycling program resumed on September 14, 1968.

Frequent mechanical difficulties with components of the fuel handling facility, however, severely hindered the recycling operations. The port selector wheel failed to position properly to the transport tube ports and on September 19, 1968, the Dosierrad jammed completely. The removal and disassembly of these units is time consuming because they are located inside the barrier gas envelope surrounding the primary system. New end-switches were installed on the port selector wheel and a stronger motor-clutch drive unit was provided for the Dosierrad. Thermal power operation of the AVR station began again on October 4, 1968 and electrical generation resumed on October 6, 1968. Plant down time, 1401 hours.

- F. October 24, 1968 - This scheduled shutdown was made to inspect possible damage to the steam turbine during a too rapid restart incident which occurred six days earlier. The inspection revealed only minor damage to the turbine and repair was deferred to the next shutdown period scheduled for March 1, 1969. Plant down time, 1 hour and 6 minutes.
- G. February 28 to April 18, 1969 - This major plant shutdown for revision and maintenance was scheduled to provide a final thorough inspection of the plant facilities prior to customer acceptance of the experimental nuclear station. The inspection and maintenance activities were carefully organized in advance to minimize the length of the outage period. The shutdown period was ushered in a day prematurely by a defective signal amplifier in the safety circuits which

triggered a reactor scram. During the ensuing outage period, the defective amplifier was repaired and a host of other maintenance and revision activities were carried out on the steam turbine, the fuel handling system and numerous components of the primary and secondary circuits.

Thermal power operation of the plant resumed on April 18, 1969 after the major revision and inspection program was completed on schedule. Initially, the thermal power level was restricted to 10 Mw while the gas purification system cleansed the primary circuit of gaseous impurities admitted during the repair and inspection activities. After three days, the impurity concentrations declined to a level where higher coolant gas temperatures could be accepted. The reactor power level was then raised to 20 Mwth giving a hot gas temperature of 600°C and permitting the transition to steam production and turbo-alternator operation. Plant down time, 1245 hours.

- H. April 21, 1969 - Following the repair of the steam turbine, the rotation of the turbine shaft was carefully checked and found to be free of imbalances. On April 21, 1969, the turbine was shut down for a short time so that the instrumentation for checking its balance could be removed. Following this, the reactor power level was gradually raised to 39 Mwth and operation of the electrical generator resumed late the same day. Plant down time, 10 hours.
- I. May 10 to May 13, 1969 - The reactor was shut down to repair the main coolant water pump which had been damaged by insufficient bearing lubrication. Plant down time, 72 hours.
- J. May 15 to May 16, 1969 - This shutdown was made to replace a defective steam pressure regulator for the turbine. Plant down time, 26 hours and 31 minutes.
- K. June 9 to June 13, 1969 - This shutdown was required to repair the port selector wheel of the fuel handling facility. Plant down time, 79 hours.
- L. June 18, 1969 - Power operation was interrupted to conduct a rod drop experiment to measure the reactivity worth of the four shutdown rods. Plant down time, 15 hours and 8 minutes.

- M. July 30 to August 6, 1969 - This plant shutdown was made to carry out dynamics experiments and to provide time for needed maintenance work. The dynamics experiments consisted of a rod drop experiment to measure the worth of the four shutdown rods. Following this, one of the rods was fully withdrawn from the core to simulate a "jammed rod" incident and to ascertain how long the reactor would remain subcritical if only three rods were available for insertion during full power operation. This information, together with knowledge of the power level to which the reactor will stabilize after return to critical, is important in assessing the seriousness of an actual accident of this type. It was concluded that at least several weeks would be available to provide supplementary shutdown reactivity in the event of an actual single rod failure. Plant down time, 177 hours.
- N. August 9 to September 3, 1969 - A gas flow blockage developed in the low temperature sections of the RA-4a and RA-4b purification stages on August 8, 1969 due to frozen moisture. The units were taken out of operation when the pressure drop across them reached three atmospheres and reactor power operation continued for a short time without these stages of the purification facility. The rising concentration of water vapor in the primary circuit, however, made it necessary to shut down the reactor on August 9, 1969. The source of the water is believed to have been from the barrier gas system which encloses the primary reactor pressure vessel. Shortly before the flow blockage developed, a portion of this gas had been routed to the RA-4 unit for purification.

The gas intake lines to the RA-4 unit were dried by auxilliary heating and on August 19, 1969 the reactor was brought to a power level of Mwth to facilitate drying of the primary circuit. On August 26, 1969, during the removal of the auxilliary heating equipment, traces of hydraulic oil were found on some gas valves. The reactor was shut down and the source of the oil was found to be ruptured diaphragms in compressors MK-6a/b. Cleansing operations with carbon tetrachloride and a nitrogen drying stream began at once. This work was completed on September 3, 1969 and electrical power generation resumed at that time. Plant down time, 611 hours.

- O. January 19, 1970 - A rod drop experiment was performed to measure the reactivity worth of the four shutdown rods. Plant down time, 2 hours and 23 minutes.
- P. January 20 to January 25, 1970 - The plant was shut down for routine maintenance work, notably the replacement of electrical coils on the magnetic valves of gas circuits inside the containment vessel. In addition, the subcriticality of the reactor, with all four shutdown rods inserted and the core at a temperature of 105°C, was measured. The scrap separator unit of the fuel handling facility was inspected and found to be not functioning properly. It was then disassembled and 350 undamaged spheres were found to have accumulated in the hopper arrangement located beneath the rotating drum of the unit.

The spheres were removed from the hopper by means of a provisory suction device. The malfunction was found to have arisen from the increased coefficient of friction between the graphite spheres and steel caused by the dry helium atmosphere and excessive axial play in the rotating drum. When it became apparent that extensive repairs were required, including the construction and installation of a completely new and improved rotating drum unit, the reactor was brought back to power to avoid a prolonged shutdown period. Repair work on the scrap separator unit proceeded concurrently with reactor power operation. Plant down time, 139 hours and 20 minutes.

- Q. March 6 to March 13, 1970 - The reactor power gradually declined from 14.5 Mwe in January to 9 Mwe in early March because of fuel burnup. Since the fuel handling facility was out of commission, it was impossible to carry out normal fuel loading and recycling and on March 6, 1970 the power station had to be shut down.

The repairs to the scrap separator were completed and completely satisfactory operation of the fuel handling facility has since been obtained. The outage period was prolonged by the necessity of pumping the reactor coolant gas inventory into storage so that a defective valve isolating the scrap separator from the primary circuit could be replaced. Plant down time, 177 hours and 48 minutes.

DISCUSSION

R. C. Dahlberg: Please comment on the type of operator errors which cause unscheduled shutdowns.

H. Knüfer: Failures are not only caused by operators that use shift people, but about 2/3 by maintenance people and 1/3 by shift people. Example by maintenance people: an electrician drew the electronic card for the main blowers instead of an auxiliary blower. Example by shift people: an operator opened a wrong valve for depressurising a small room of the blocking-gas-system. This caused a pressure drop for the helium pressure control system and dropped a rod. Some possible ways to minimize human failures (to abolish them is impossible): 1) Minimizing maintenance work that could influence operation, 2) installation of more warning signals, and 3) introduction of more check lists.

John Kemper: Why the search for tritium? I thought that HTGR's were especially fortunate in this respect.

G. Ivens: Yes, you are quite correct in saying that tritium production is especially low in HTGR's. The tritium present in the primary circuit can be easily handled by the gas purification plant. Our interest was to find out the origin of the small amount of tritium which has been detected in the steam circuit.

P. Cohen: In future experiments on tritium it might be useful to add tritium as HTO.

E. Hoinkis: What is the iodine concentration in the primary coolant circuit?

G. Ivens: There is no detectable level of iodine in the AVR primary coolant gas. Furthermore, no iodine could be detected either on graphite spheres which are removed from the core or on steel surfaces of components in direct contact with the coolant gas. Unfortunately, the most interesting surfaces, which are in contact with the hot primary gas, are not accessible for inspection.

P. S. Weltevreden: Comparing the impurity levels of AVR and Dragon it appears that the AVR level are orders of magnitude higher than those of Dragon.

G. Ivens: The gaseous impurities in the AVR primary circuit were initially high because of the presence of more than 100 tons of carbon brick structural material. This ungraphitized carbonaceous material emits large quantities of H_2O and CO_2 when heated. The power level of the early AVR operations was restricted because of the presence of these oxidizing impurities. In the meantime, outgassing has proceeded to the point that the AVR power level is no longer limited by these impurities. It should be noted, however, that in a pebble bed reactor corrosion is distributed evenly amongst the fuel elements because they are in continuous motion.

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OBJECTIVES AND PLANS FOR FUEL TESTING
IN THE PEACH BOTTOM HTGR*

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ABSTRACT

The Peach Bottom reactor offers unique capabilities as a test facility for HTGR fuels. It is the only reactor in the United States that can irradiate HTGR fuel in a proper neutron spectrum, in a pressurized helium environment, and in an HTGR temperature regime. In addition, the Peach Bottom reactor can accommodate kilogram quantities of test fuels for long periods, and complete fuel element assemblies can be tested to evaluate the interactions of fuel particles, fuel beds, and graphite structures. One of the fundamental objectives of Peach Bottom Core 2, therefore, is to exploit the unique capabilities of this reactor as a test facility for irradiation of HTGR fuel concepts.

Gulf General Atomic plans initially to design and fabricate four types of test elements for insertion with Core 2. Up to 40 test elements may be irradiated within the core at any one time. The major objectives of each element type are as follows:

1. Fuel Test Elements (FTEs). To evaluate the performance of HTGR fuels over a wide range of operating conditions
2. Recycle Fuel Test Elements (RTEs). To provide irradiated HTGR fuel in sufficient quantities to develop recycle processing technology, and to test recycle particles
3. Fuel Bed Test Elements (FBTEs). To provide large-scale tests of 1100-MW(e) HTGR fuel
4. Proof Test Elements (PTEs). To demonstrate irradiation performance of Fort St. Vrain fuel

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Emphasis is being placed on the first three element types since these have the greatest relevance to 1100-MW(e) HTGR technology. In particular, the FTEs will be used (1) to determine the time-at-temperature lifetimes of various types of fuel, (2) to obtain data to identify mechanisms of particle failure and thereby to improve fuel performance and lifetimes, (3) to test loose particle graphite inter-active forces in blended beds, and (4) to conduct controlled irradiation experiments on fuel particle thermal stability, metallic diffusion rates, fission product release, and graphite mechanical properties.

Experiments will be conducted as functions of temperature and fluence up to 4.2×10^{21} n/cm²; burnups will be ~20% FIMA in the mixed fissile and ~60% FIMA in the UC₂ fissile particles at end of life. Some thermal stability samples will be irradiated at temperatures from 1000° to 1600°C in thermal gradients up to 1000°C/in. in order to determine time-temperature limits for coated particles under irradiation. Extensive postirradiation examination of the FTEs will be performed. One of the FTEs to be irradiated for 3 yr is being sponsored by the USAEC.

Objectives of the FBTEs, in addition to large-scale testing of 1100-MW(e) fuel, are (1) to evaluate various 1100-MW(e) bonded bed concepts, and (2) to determine the dimensional changes of blended beds of reference 1100-MW(e) fuel by periodic gamma scanning. All of these elements will be irradiated for 3 yr and will receive a dose of approximately 4×10^{21} n/cm² during this time. Most of the FBTEs will contain only one fuel combination and may be monitored continually during irradiation by sampling the individual purge gas streams.

The RTEs are sponsored by the USAEC as an integral part of the National HTGR Recycle Program. They include both reference and advanced concepts for 1100-MW(e) recycle fuel, primarily as bonded rods, although some blended beds will also be included. These elements will provide large-scale tests of oxide fuel produced by the sol-gel process and will constitute proof tests of the recycle 4:2(Th,U)O₂ BISO particles. In addition, 2:0(Th,U)O₂ BISO particles will be irradiated, attaining the burnup after 3 yr in the Peach Bottom reactor that the other particles would receive after 4 yr in the 1100-MW(e) HTGR. The RTEs will provide irradiated material for head end reprocessing studies at ORNL after 1-, 2-, and 3-yr irradiations in Core 2.

1. INTRODUCTION

The Peach Bottom reactor, operated by Philadelphia Electric Company, offers unique capabilities as a test facility for high-temperature gas-cooled reactor (HTGR) fuels. It is the only reactor in the United States that can irradiate HTGR fuel in a representative HTGR neutron spectrum and helium coolant environment. In addition, the Peach Bottom reactor can accommodate kilogram quantities of test fuels for long irradiation periods, and full-scale fuel element assemblies can be tested to evaluate the interactions of fuel particles, fuel beds, and graphite structures. One of the fundamental objectives of Peach Bottom Core 2 (the second core to be loaded into Peach Bottom), therefore, is to utilize the unique capabilities of this reactor as a test facility for irradiation of HTGR fuel concepts. Table 1 compares light water reactor and HTGR environments and indicates the suitability of the Peach Bottom reactor for irradiating HTGR fuels.

Gulf General Atomic (GGA) has designed and is fabricating four types of test elements for insertion with Core 2. Up to 40 test elements may be irradiated within the core at any one time. The element types are listed below, and the chief objectives of each are given in Table 2.

Fuel test elements (FTEs)

Recycle fuel test elements (RTEs)

Fuel bed test elements (FBTEs)

Proof test elements (PTEs)

The first three element types have basically the same design, with the fuel being contained in cylindrical graphite bodies within regular Peach Bottom fuel element sleeves. In contrast, the PTE design simulates a section of a large-HTGR fuel block and is hexagonal in shape. Emphasis is being placed on the FTEs, RTEs, and FBTEs, since these elements have greater flexibility for testing fuel under specially controlled conditions. These elements are sponsored both privately by GGA and by the Atomic

Table 1. Comparison of Reactor Operating Conditions

	H ₂ O Test Reactor (ETR)	Peach Bottom HTGR	1100-MW(e) HTGR
Environment	Low-pressure static helium	High-pressure flowing helium	High-pressure flowing helium
Neutron spectrum	Light-water reactor (LWR)	HTGR	HTGR
Thermal flux (n/cm ² -sec)	2x10 ¹⁴	4x10 ¹³	4x10 ¹³
Fast flux (n/cm ² -sec)	4x10 ¹⁴	4x10 ¹³	4x10 ¹³
Core Th:U ratio	2:1	8:1	4:1
Peak fuel temp. (°C)	>1300	>1300	1340
Thermal gradient	High	Moderate	Moderate
Burnup rate	High	Moderate	Moderate

Table 2. Chief Objectives of Various Test Elements

Fuel test elements (FTEs)	<ol style="list-style-type: none"> 1. Evaluate the performance of HTGR fuels over a wide range of operating conditions and lifetimes <ol style="list-style-type: none"> a. Determine the time-at-temperature lifetimes of various types of fuel b. Obtain data to identify mechanisms of particle deterioration under extreme conditions and thereby to improve fuel performance and lifetimes c. Evaluate various production processes 2. Conduct controlled irradiation experiments on: <ol style="list-style-type: none"> a. Fuel particle thermal stability b. Metallic diffusion rates in an HTGR environment c. Fission product release of various types of fuel particles d. Graphite mechanical and creep properties e. Various fuel rod fabrication concepts 3. Evaluate the long-term, in-pile performance of blended beds and bonded rods under HTGR conditions
Recycle fuel test elements (RTEs)	<ol style="list-style-type: none"> 1. Demonstrate performance of recycle type fuel and beds under actual HTGR operating conditions 2. Produce significant quantities of irradiated HTGR fuel for reprocessing studies at UKNL, and subsequently to: <ol style="list-style-type: none"> a. Demonstrate separability of particles. b. Prove out reprocessing equipment
Fuel bed test elements (FBTEs)	<ol style="list-style-type: none"> 1. Demonstrate performance of large-HTGR fuel beds under actual reactor operating conditions 2. Provide bonded and blended bed elements containing single fuel combinations for: <ol style="list-style-type: none"> a. Periodic monitoring for fission gas release b. Periodic gamma scanning for dimensional changes
Proof test elements (PTEs)	<ol style="list-style-type: none"> 1. Demonstrate irradiation performance of reference Fort St. Vrain fuel

Energy Commission. In addition, testing of PTE-2, which has received 150 equivalent full-power days (EFPD) of irradiation in Core 1, will continue in Core 2. Another proof test element (PTE-3) is scheduled for insertion after 300 EFPD. Both of these PTEs are designed to fulfill the proof test requirements for Fort. St. Vrain (FSV) fuel, and are being funded under the Public Service of Colorado (PSC) development contract.

2. DESIGN OF TEST ELEMENTS

Fuel Test Elements

The FTEs are designed to replace instrumented and non-instrumented Peach Bottom Core 2 fuel elements. Externally, these elements have the same appearance as standard Core 2 fuel elements, and their basic arrangement and design are also similar (Figs. 1 and 2).

The top reflector, sleeve, and bottom connector components, which form the outer containment envelope of each test element, are standard Core 2 fuel element components. The lower reflector and internal trap are very similar to the standard fuel element components. The active fuel portion is made up of long, cylindrical H-327* graphite fuel bodies which replace the fuel compacts in the Core 2 fuel element. Each test element comprises either three 31-in.-long fuel bodies or six 15.5-in.-long fuel bodies, depending on the specific test objectives. Eight axial purge grooves are present on the outside diameter of each fuel body. The fuel bodies contain eight 1/2-in.-diameter fuel holes equally spaced on a circular pattern and are filled with coated fuel particles, either in the form of fuel rods or blended beds (Fig. 1). The sleeve, top reflector, and bottom connector assembly constitute a completely closed containment around the fuel bodies.

A central (spine), 1.1-in.-diameter hole is provided for controlled irradiation testing of various sample types. These center (spine) samples are enclosed within their own containers and include fuel particles and

*Manufactured by Great Lakes Carbon Corporation.

fuel rods, graphite samples, metallic diffusion samples, and fission product release samples. When samples are not included, the center irradiation hole contains a graphite spine.

Each test element is cooled by the flow of helium in the tricuspid channels between fuel elements in the same manner as the Core 2 elements, and is purged in the same basic manner as the Core 2 fuel elements. All the purge gas flows in the purge grooves and the annular clearance gap between the fuel body outside diameter and sleeve inside diameter. Seven test elements will be located in special positions within the core at which the purge gas stream can be sampled periodically for release of fission products.

Approximately 12 of the test elements occupy instrumented locations within the core. Each of these elements has a tungsten-rhenium and a Chromel-Alumel thermocouple located at the plane of maximum temperature of the element. The core locations of the remaining uninstrumented test elements have been selected to meet the objective of maintaining relatively constant power density within these elements during their irradiation lifetime. Axial and radial temperature profiles in a representative element are shown in Figs. 3 and 4.

Proof Test Elements

The PTEs are designed to replace standard instrumented Core 2 fuel elements. Externally, they are different from Core 2 elements, having a hexagonal shape over the fueled region (Fig. 1). Also, these elements are cooled both internally and externally, and there is no purge stream to remove released fission products.

Each element consists of seven sections joined together. From bottom to top, these sections consist of a bottom connector, a bottom reflector, four fuel zones, and a top reflector. The bottom portion of the element is cylindrical with a transition to a hexagonal shape in the bottom reflector section. The active fuel zones are hexagonal, with the

width across the flats equal to the diameter of the intermediate spacers of the regular elements. The test element length is the same as for a standard Peach Bottom element. As shown in plan view in Fig. 1, the element has seven internal coolant channels and 12 fuel rod holes distributed within the hexagonal graphite body. Cooling is provided both by the seven internal coolant channels and the six modified tricuspid channels adjacent to the element. The elements are not purged, but are instrumented with two thermocouples whose junctions are located 53 in. above the bottom of the active core. A Chromel-Alumel thermocouple is located in the graphite body, and a tungsten-rhenium thermocouple is located in the inner ring of holes within an annular fuel rod.

The active portion of the fuel element is composed of four fuel zones joined together axially by cemented threaded joints. Each fuel zone contains 12 blind fuel holes drilled from the top of the zone to within about 0.4 in. of the bottom. The diameter of the regular fuel holes is 0.470 in. The fuel holes containing the annular fuel rods with the thermocouples have a diameter of 0.517 in. The seven coolant channels are continuous from the top to the bottom of the zone, and they are aligned with the channels in adjacent zones to form continuous internal coolant channels over the full length of the fuel element.

PTE-2, originally irradiated in Core 1, was not removed from the core and will continue irradiation in Core 2. Axial and radial temperature profiles during operation are shown in Figs. 5 and 6.

3. HTGR FUEL LOADING COMBINATIONS

Present HTGRs employ carbon-coated uranium and thorium carbide spheres as their fuel. These fuel particles, which can vary from 250 to 1000 μm in diameter, are bonded into fuel rods or compacts and are subsequently loaded into graphite elements or blocks. A possible alternative for future HTGRs is to load the blocks with blended beds of loose particles. In general, it is desired to gain a better understanding of HTGR fuel design technology in these areas and assess the properties and

performance of various types of coated particles, fuel rods, and blended beds under irradiation. The loadings of the test elements are therefore dictated by the fuel loadings proposed for current and future HTGRs.

The fuel particles considered are of the same general type, having spherical carbide or oxide kernels, a porous "buffer" layer of pyrolytic carbon (PyC) to absorb fission gases, attenuate fission recoils, and allow for kernel swelling, and an outer layer of relatively dense isotropic PyC to retain fission gases. Such particles are termed BISO by GGA; TRISO particles are similar but include an extra layer of dense pyrolytic silicon carbide sandwiched between two layers of isotropic PyC to retain metallic fission products. Cross sections of BISO and TRISO particles are shown in Fig. 7. Particles may be either fissile (containing U^{235}) or fertile (containing only Th^{232}), and their size depends on the kernel type and particular reactor application.

The standard elements in Peach Bottom Core 2 will be loaded with BISO fissile particles in the form of compacts. The Fort St. Vrain core will be loaded with TRISO fissile and TRISO fertile particles in the form of fuel rods. The 1100-MW(e) HTGR will initially be loaded with both TRISO and BISO fissile particles and BISO fertile particles in the form of rods (see Fig. 8). The test element program is aimed primarily toward furthering 1100-MW(e) HTGR technology, although advanced knowledge of the performance of Fort St. Vrain fuels will be obtained from the bonded TRISO/TRISO fuel included in the program. The discussion below is restricted to the 1100-MW(e) HTGR startup and recycle modes of operation.

In the 1100-MW(e) HTGR, two types of graphite block loadings will be used (see Fig. 8). The particles in the A-type block can be processed together after irradiation, but the particles in the B-type block require separation. This separation is necessary because fissionable U^{233} is bred into the fertile particles and is recycled in the $(Th, U^{233})O_2$ BISO recycle particles. However, large quantities of U^{236} are produced in the

$U^{235}C_2$ TRISO particles, and continuous recycle of this material is not desired. The amount of U^{236} bred into the $U^{235}C_2$ BISO particles in the startup A block is relatively small and is progressively diluted with every recycle; these particles therefore do not need separating. In the FTEs and FBTEs, emphasis is placed upon the performance of the startup A and B block combinations, both as rods and loose particles. The RTEs emphasize only the various recycle block loadings and possible alternatives to the reference loadings shown in Fig. 8. The loading combinations being investigated in the various elements and the justifications for their inclusion are given in Table 3.

4. RECYCLE FUEL TEST ELEMENTS

Eight RTEs are being fabricated for insertion in Peach Bottom Core 2. These elements are funded by the USAEC as an integral part of the National HTGR Recycle Program, and are being fabricated jointly by GGA and Oak Ridge National Laboratory (ORNL). The main objectives of the elements are listed in Table 2. Two of the RTEs are to be irradiated for 1 yr, two for 2 yr, and four for 3 yr (see Fig. 9). A 1-yr irradiation in the Peach Bottom HTGR is roughly 300 EFPD. Periodic visual and gamma scanning will be conducted on the elements, but they will not be instrumented. The elements will be shipped to ORNL for postirradiation examination and subsequent studies after discharge from the Peach Bottom reactor.

Each element consists of six 15.5-in. graphite bodies in which the uranium loading is determined by the position in the reactor. The fuel particle combinations and locations in the elements have been selected to produce irradiated fuel bed materials which will have been exposed to a wide range of reactor environmental conditions. These fuel bed materials will be used segment by segment to test and develop fuel reprocessing equipment and processes. The unit operational processes which will be investigated include size reduction, size separation, burning, chemical dissolution, leaching, and solids transport. In addition, the elements will provide proof tests of sol-gel oxide fuel particles and, in particular, the reference recycle $(Th,U)O_2$ fuel particles.

Table 3. Fuel Loading Combinations in Advanced Test Elements

Designation	Combination	Type of Loading	Justification
RTEs			
a	(4.2 Th,U) O_2 BISO	Loose & bonded	Reference recycle particle; will check out bed sticking, chemical separation, etc.
b	UO $_2$ BISO + ThC $_2$ BISO	Bonded	Backup recycle A block loading if Th is eliminated from (Th,U) O_2 particles
c	(2.0 Th,U) O_2 BISO + ThO $_2$ BISO	Bonded	Alternate recycle A block loading; 2:1 ratio to obtain end-of-life burnup (20%) in 3 yr. ThO $_2$ proposed if oxide process adopted.
d	(2.0 Th,U)C $_2$ BISO + ThC $_2$ BISO	Bonded	Recycle A block loading; 2:1 ratio to obtain 20% burnup in 3 yr
e	UC $_2$ BISO + ThC $_2$ BISO	Loose & bonded	Startup A block loading
f	UC $_2$ TRISO + ThC $_2$ BISO	Loose & bonded	Startup and recycle B block loading
g	UO $_2$ BISO + ThO $_2$ BISO	Bonded	Backup B block loading if Th is eliminated and if sol-gel ThO $_2$ is used
h	UC $_2$ BISO + ThO $_2$ BISO	Bonded	Backup A or B block loading; separable chemically or by selective burning
i	UC $_2$ TRISO + ThC $_2$ TRISO	Bonded	Alternate B block loading if cleaner primary circuits required
FTEs and FBTEs			
e	UC $_2$ BISO + ThC $_2$ BISO	Loose & bonded	Startup A block loading
f	UC $_2$ TRISO + ThC $_2$ BISO	Loose & bonded	Startup and recycle B block loading
k	(2.0 Th,U)C $_2$ BISO + ThC $_2$ BISO	Loose & bonded	Recycle A block loading with (Th,U)C $_2$ substituting for (Th,U) O_2
l	(2.0 Th,U)C $_2$ TRISO + ThC $_2$ TRISO	Loose & bonded	Reference Fort St. Vrain loading

Table 4. RTE Loading Combinations^a

Position of Fuel Body in Element	Center Line Temp Range (°F)	First Discharge		Second Discharge		Final Discharge			
		RTE 1, All Bonded (Quartered)	RTE 2	RTE 3, All Bonded (Quartered)	RTE 4	RTE 5, All Bonded (Quartered)	RTE 6, All Bonded	RTE 7, All Bonded (Quartered)	RTE 8
6 (top of reactor)	2050-1950	bdei	f	bdei	e ^b	bdei	g	acfg	f
5	2250-2050	bdeh	a ^b	bdeh	d	bdeh	f	acfg	i
4 (max. flux)	2250-2300	acfg	e	acfg	f	acfg	f	bdeh	d
3 (max. flux)	2050-2250	acfg	f ^b	acfg	a ^b	acfg	d	bdhi	e
2	1650-2050	acfg	d	acfg	f ^b	acfg	f	bdhi	i
1 (bottom of reactor)	1050-1650	acfg	e ^b	acfg	e	acfg	e	bdhi	f ^b

^aLoadings are indicated by letters a, b, c, d, e, f, g, h, and i, and are defined in Table 3.^bBlended beds.

The proposed loadings of the RTEs are shown in Table 4. In RTEs 1, 3, 5, and 7, all six bodies are "quartered," and contain a different fuel combination in every two holes of a given body. These elements will be used for small-scale reprocessing in the ORNL hot cell. RTEs 2, 4, 6, and 8, which contain only single loadings in a given body, will provide larger amounts of the chief combinations of interest for hot cell work and also will provide the initial irradiated material required for reprocessing in the Thorium-Uranium Recycle Facility (TURF). Additionally, some blended beds will be irradiated in RTEs 2, 4, and 8, since these elements constitute a backup design for fuel rods in the HTGR.

Fuel for the RTEs is being fabricated by both ORNL and GGA. ORNL has produced and coated all the sol-gel oxide kernels, and GGA has produced and coated the various carbide kernels. The particles have been made to tentative 1100-MW(e) HTGR specifications. Fuel rods with different matrices are being fabricated for the RTEs both at GGA and ORNL by means of the hot injection molding process. Blended beds are being loaded at GGA. All element assembly will take place at GGA, and it is planned to ship the elements to Peach Bottom by May 15.

5. FUEL BED TEST ELEMENTS

Six FBTEs are being fabricated for insertion in Peach Bottom Core 2. The main objectives of these elements, which are funded privately by GGA, are given in Table 2. It is planned to irradiate all of these elements for 3 yr (900 EFPD), and to conduct periodic visual and gamma scanning. Gamma scanning will reveal dimensional changes of the beds, particularly in the case of FBTEs 5 and 6, which are loose bed elements. All six FBTEs will be instrumented and will be installed on stand-off pins where the purge gas stream can be monitored periodically during irradiation. In-pile fission product release data can thus be obtained from these elements. The mixed element (FBTE-5), which is complementary to FBTEs 1 and 2 (see Section 6), will contain graphite spine samples and will receive postirradiation examination; the other elements will contain no spine samples and may be used for reprocessing studies at ORNL after discharge.

Each element consists of three 31-in. bodies rather than six 15.5-in. bodies as in the RTEs. The proposed fuel particle loading combinations are shown in Table 5, and in each case these are relevant to the 1100-MW(e) HTGR loadings. The UC_2 BISO + ThC_2 BISO and UC_2 TRISO + ThC_2 BISO bonded combinations represent the startup mode for the 1100-MW(e) HTGR (Fig. 8), and $(Th,U)C_2$ BISO + ThC_2 BISO represents the recycle A block loading with carbide rather than oxide fissile kernels. Bonded $(Th,U)C_2$ TRISO + ThC_2 TRISO particles are to be used in Fort St. Vrain, and this particular element will therefore provide advanced knowledge of the performance of such bonded fuel. In addition, the fission gas release data obtained by purge stream sampling will provide continuing surveillance of Fort St. Vrain operating conditions. Only a single fuel combination is to be used throughout FBTEs 1, 2, 3, 4, and 6 in order that the fission gas release data can be directly related to the performance of a particular type of fuel.

The outer bed loadings of the mixed element FBTE-5, which are the same as in FTEs 1 and 2, are shown in Fig. 10. This is a blended bed element in which the particles are mixed with calcined petroleum coke ($\sim 0.15 \text{ g/cm}^3$ of fuel hole). Body 1 contains particles with propylene-derived coatings which are relevant to the Fort St. Vrain and 1100-MW(e) HTGRs. These particles have relatively high PyC coating densities (1.70 to 1.80 g/cm^3 for TRISO and 1.80 to 1.90 g/cm^3 for BISO). In bodies 2 and 3, the effect of PyC densification rate on the rheological problems of blended beds will be investigated. The high-density PyC in this case is 1.80 to 1.90 g/cm^3 and the low-density PyC is 1.55 to 1.65 g/cm^3 . The rate at which stresses in blended beds build up may vary with the PyC densification rate on the particles, the latter rate being higher, the lower the PyC density for doses up to about $4 \times 10^{21} \text{ n/cm}^2$. Owing to such uncertainty of blended bed behavior, the 3-yr irradiation spine samples in FTE-3 will be contained in a bonded element, and FBTE-5 will demonstrate the 3-yr irradiation behavior of such beds. It is anticipated that particle/graphite interaction stresses will not build up significantly until at least the third year of irradiation. If necessary, however, FBTE-5 could be removed from the reactor for postirradiation examination at any suitable time, since it will contain no critical spine samples.

6. FUEL TEST ELEMENTS

Six FTEs are being fabricated for insertion with Peach Bottom Core 2. The overall objectives of these elements are given in Table 2; more specific objectives of the various spine samples are discussed in the following sections. The elements will contain both bonded rods and blended beds as the driver fuel, and controlled experiments will be conducted in the spines. Two of the FTEs will be irradiated for 1 yr, two for 2 yr, and two for 3 yr (see Fig. 9). This schedule was chosen primarily to assess the effects of irradiation dose on the performance of the blended beds, bonded rods, and spine samples. All the elements will be instrumented, and FTE-6 will be at a purge sampling position. Postirradiation examinations of all the elements will be conducted at GGA.

Outer Bed Loadings

Each element consists of three 31-in. bodies. The loadings of FTEs 3, 4, 5, and 6 are shown in Fig. 11; and the outer bed loadings of FTEs 1 and 2 are shown in Fig. 10. FTEs 1 and 2 use blended beds as described in Section 5, while FTE-3 uses bonded rods containing the same particles. The loadings of FTEs 4, 5, and 6 are identical, and the particles are the same as those irradiated in FBTEs 1-4. Half of the fuel rods in a given body will be bonded with an isotropic coke + pitch, and the remainder will be bonded with a natural flake graphite + pitch. Since any particular combination and matrix runs the complete length of any given element, it will be possible to determine the effects of temperature, flux, and fluence up to 4.2×10^{21} n/cm² on the irradiation performance of these rods. Likewise, the effects of the same variables on blended bed performance can be checked in FTEs 1 and 2 and FBTE-5.

Spine Samples

Experimental samples in the FTEs are contained in the central 1.10-in.-

diameter spine hole. In general, the spine sample program can be divided into five categories according to the samples inserted and the test objectives:

1. Thermal stability of fuel particles
2. Fission product release from fuel particles
3. Metallic fission product diffusion through graphite
4. Isothermal proof testing of fuel rods
5. Physical property changes of graphites

The specimens differ in design, but they are all contained within 1-in.-diameter cylindrical crucibles having sufficient clearance within the fuel body center hole to prevent interference with the graphite body at any time during irradiation. By using these crucibles, all the center samples are in effect triply contained, and the experiments requiring more precise posttesting analysis (thermal stability and fission product release) employ smaller containers inside these crucibles. The purpose and description of each type of experimental sample are presented below, and the principal experimental variables being investigated with each type of sample are discussed. In all cases, the experiments are repeated in 1-, 2-, and 3-yr irradiation elements, often at different temperatures so that the effects of irradiation dose and temperature can be assessed.

Fuel Particle Thermal Stability Samples

The purposes of these experiments are:

1. To expose fuel particles to a wide range of operating variables, particularly high temperatures and temperature gradients
2. To determine the time-at-temperature lifetimes of various types of fuel
3. To obtain data which can, when integrated with other work, lead to a better understanding of particle deterioration and failure under extreme conditions

In general, useful data concerning particle performance and design limits can be obtained only by exposing various particle types to conditions severe

enough to cause failure of some of the particles. This can be achieved by combining abnormally high irradiation temperatures and thermal gradients which can cause kernel migration, kernel-coating interaction, and subsequent particle coating failure.

The most satisfactory way to achieve high temperatures and high thermal gradients in the test elements is to use the sample design shown in Fig. 12. Particles are loaded individually into separate holes of the inner graphite crucibles. These crucibles slide into the cylindrical spine crucible, which is then loaded with loose particles, and a lid is screwed into place. The amount of uranium in the particles and the axial position of the crucible in the element determine the maximum temperature and temperature gradient experienced by the particles in the inner crucibles. Several of the inner crucibles are assembled within each 1-in.-diameter crucible. The maximum temperature of these crucibles will be about 1500°C, and temperature gradients will be up to 1000°C/in. Some particles in these crucibles are loose and others are bonded in place. The experiment is designed to provide conditions under which kernel migration can occur and possibly result in the failure of some of the particle coatings in the inner crucibles, certain of which have been especially selected because of high migration rates observed in out-of-pile tests. Some other, simpler crucibles are also being used to irradiate particles from the same batches under almost isothermal conditions.

Variables other than fluence and temperature that are being investigated can be conveniently divided into two categories: kernel variables and coating variables. Kernel variables include:

Kernel type	UC_2 , $(Th,U)C_2$, ThC_2 , UO_2 , $(Th,U)O_2$, ThO_2
Th:U ratio	Zero, 2:1, 4:1, 18:1
Impurity content. . . .	Various amounts
Hydrogen content. . . .	Kernels deliberately hydrolyzed before coating

Coating variables include:

- BISO and TRISO type coatings
- Different coating gases
- High and low coating densities

The crucibles were radiographed before and will be radiographed after irradiation in hopes of revealing kernel and coating dimensional changes, kernel migration, and particle failure. Other postirradiation examination tests will include photomacrography, gamma scanning, autoradiography, metallography, and electron microprobe analysis.

Fuel Particle Fission Product Release Samples

The purpose of these experiments is to obtain the following information:

1. In-pile and out-of-pile fission product release data for BISO and TRISO fuel particles as a function of burnup and temperature
2. Distribution coefficients of fission products between kernels and coated particles
3. Kernel release data as a function of burnup and temperature
4. The effect of different types of kernels (fissile, fertile, oxides, carbides) on fission product release
5. Thorium and uranium migration in kernel and coating as a function of temperature
6. The effect of metal doping of the coatings on the fission product release

The design of graphite crucibles for fission product release samples is shown schematically in Fig. 13. A few hundred loose particles are loaded into each crucible along with graphite flour to absorb fission products. The crucible is in turn contained within an outer graphite crucible approximately 1 in. in diameter by 1.25 in. long. Some of the small crucibles are contained within thick-walled niobium cans ($\sim 1/4$ in. thick), as shown in Fig. 14. The object of the niobium cans is to retain all the fission products released during irradiation within the crucible, and this can be achieved only with a sealed can. However, reactor physics considerations limit the amount of metal which can be placed in the core, and therefore graphite outer crucibles have been used for most of these samples. The variables being investigated with the fission product release samples are:

BISO versus TRISO coatings

UC_2 , $(Th,U)C_2$, ThC_2 , UO_2 , $(Th,U)O_2$, ThO_2 kernels

Effect of doping of fissile particle coatings

Postirradiation examination of the fission product release samples, which will be carried out in the Chemistry Department facilities at GGA rather than the Hot Cell, will include:

1. Fission gas release and fission product release measurements
2. Determinations of fission product concentrations between kernels, coatings, graphite, and charcoal, leading to in-pile release data
3. Postirradiation anneals to confirm release data
4. Postactivation analyses for Sr^{91} release
5. Bare kernel release measurements
6. Fission product release measurements on doped particles

Metallic Diffusion Samples

The purpose of the metal diffusion studies is to obtain:

1. Migration rates for strontium, cesium, barium, samarium, etc., in graphite under reactor conditions as a function of metal concentration and temperature
2. Data on the effects of the presence of other fission product metals on the migration rate of individual metals (especially strontium)
3. Data on the ability of getter materials to reduce metal migration. (Getter materials combine with fission products such as strontium to inhibit their diffusion into the reactor system.)
4. Data on the integrity of getter materials

A typical graphite crucible for the diffusion samples is shown in Fig. 15. The graphite crucibles are about 1.25 in. long and 1.0 in. in outside diameter. The annular hole contains various metals (such as strontium, cesium, and barium) in the form of carbides, mixed in known proportions in a graphite matrix material.

Variables to be investigated include different fission product elements and element concentrations, combinations of various elements, and getters. Postirradiation examination will include:

1. Sectioning of crucibles and determination of metal concentrations across the crucible walls
2. Analyses of the source materials to determine total metal loss
3. Calculations of migration rates for each metal
4. Determination of the effects of getters
5. Investigations of getter integrity

Bonded Rod Samples

Bonded rod samples are included to evaluate the performance of various types of bonded fuel rod concepts as functions of temperature and irradiation dose, and thereby to determine the best overall fuel rod concept for use in future HTGRs.

Bonded rods are approximately 1/2 in. in diameter and from 3/4 in. to 2 in. long. Each rod is inserted in a 1-in.-diameter graphite crucible with a screw-on top. The variables being investigated include filler type, production method, and packing fraction.

Postirradiation examination of the samples will include visual examination, dimensional change measurement, photomacrography, metallography, and possibly burn/leach testing for SiC integrity.

Graphite Samples

The purposes of the graphite sample irradiations are as follows:

1. To determine the effect of irradiation dose and temperature on the tensile properties of various graphites
2. To investigate the dimensional change behavior of various graphite samples

3. To evaluate the transient creep characteristics of various graphites as functions of irradiation dose and temperature

Graphite samples are either cylindrical (tensile test pieces) or flat (dimensional change pieces) or of the general design shown in Fig. 16 (transient creep samples). In each case, the samples are contained in graphite crucibles much as in the case of the bonded rod samples described above. In each of the different sample types, it is proposed to investigate several different variables other than irradiation dose and temperature, including:

1. Isotropic and needle-coke graphites
2. Parallel and transverse orientation in the log
3. Edge and center of log
4. Various sample sizes

The postirradiation examination of the graphite samples will include:

1. Examination and photomacrography
2. Tensile tests at various temperatures
3. Dimensional change measurements
4. Measurement and tensile testing of transient creep samples
5. Some metallography

7. PROOF TEST ELEMENTS

One PTE (PTE-2) will be in Core 2 at startup. This element was irradiated in Core 1 and has not been removed from its original position. It is intended to leave PTE-2 in Core 2 for a further 300 EFPD. Another PTE (PTE-3) is being considered for insertion in Core 2 after 300 EFPD for a 300-EFPD irradiation period, after which time it will be discharged for postirradiation examination at GGA. These elements will demonstrate that fuel elements containing bonded TRISO/TRISO fuel particles, such as those to be used in the Fort St. Vrain HTGR, will meet their design objectives. They contain reference Fort St. Vrain TRISO particles bonded by hot injection molding using carbonaceous matrix materials.

PTE-2, which is now in the Peach Bottom reactor, contains both fuel rods and "piggyback" specimens consisting of the component materials of the fuel rods. The rods contain production TRISO particles which were made to preliminary Fort St. Vrain specifications, and were bonded by hot injection molding using a phenolic resin/graphite powder matrix. Twenty-four annular rods, 5.93 in. long, and 70 solid rods, 11.53 in. long, were fabricated using three basic types of fuel particles. These particles have different coating types:

1. HTI (high-temperature isotropic)
2. Low-density LTI (low-temperature isotropic)
3. High-density LTI

The piggyback locations contain loose particles from each of the 14 batches or blends used, HTI PyC and SiC coating strips, and matrix material of the type used in the rods. The piggyback materials are contained in graphite crucibles located in the centers of annular fuel rods. These samples are 1/8 in. in diameter and occupy a total of 17 in. Temperatures of irradiation will be 1150° to 1200°C, and total fast neutron dose received will be about 1.3×10^{21} n/cm². Some graphite disks are also being irradiated to approximately the same temperature and dose as the other samples.

8. CONCLUSIONS

In the past, GGA has emphasized the systematic testing of fuel particles, fuel rods, and graphite to full HTGR exposures in irradiation capsules. By combining observed irradiation performance and theoretical calculations, it has been possible to define the gross limits of acceptable design conditions. GGA is now emphasizing the understanding of the behavior of coated particles, rods, etc., and is attempting to define more precisely the limits of operating conditions for the various core components. For these purposes, the unique testing capabilities of the Peach Bottom reactor are desirable, since it offers a representative HTGR environment and neutron spectrum and can test large fuel samples and test assemblies at HTGR burnup rates and temperature gradients.

GGA plans to initially irradiate several test elements in Peach Bottom Core 2, including FTEs, RFTEs, FBTEs, and PTEs. These elements have different objectives, but are all designed to further large-HTGR technology. Irradiation testing and evaluation of test elements will continue for approximately 3 yr.

Table 5. Loadings of Initial FBTEs

FBTE No. ^a	Loading Combination	Fuel Configuration
1	(2.0 Th,U)C ₂ BISO + ThC ₂ BISO	Bonded rods; natural flake graphite matrix + pitch binder
2	UC ₂ BISO + ThC ₂ BISO	Bonded rods; natural flake graphite matrix + pitch binder
3	UC ₂ TRISO + ThC ₂ BISO	Bonded rods; natural flake graphite matrix + pitch binder
4	(2.0 Th,U)C ₂ TRISO + ThC ₂ TRISO	Bonded rods; natural flake graphite matrix + pitch binder
5	Mixed element (identical to FTEs 1, 2; see Fig. 10)	Blended beds (particles + coke)
6	(2.0 Th,U)C ₂ BISO + ThC ₂ BISO	Blended beds (particles + coke)

^aOther FBTEs may be inserted in the Peach Bottom reactor at later dates.

Fig. 1. Cross Sections of Fuel Test Element and Proof Test Element.

Fig. 2. Fuel Test Element Assembly.

Fig. 5. Fuel Rod Axial Temperature Profiles in PTE.

Fig. 6. Radial Temperature Profile in PTE at Plane of Maximum Fuel Temperature.

BISO

TRISO

Fig. 7. Cross Sections of Gulf Atomic Coated Particles.

INITIAL CORE

BLOCK ANo separation
required

RECYCLE CORE

BLOCK BSeparation
required

Fig. 8.

Coated Particles for the 1100-MW(e) HTGR. The reference fuel in the Fort St. Vrain and 1100-MW(e) HTGRs is in the form of bonded rods.

Fig. 9.

Irradiation Schedule for Test Elements.

ELEMENT TYPE	NUMBER	LENGTH OF IRRADIATION (YR)		
		1	2	3
RTE	2			
	2			
	4			
FBTE	6			
FTE	2			
	2			
	2			
PTE	1			
	1

Fig. 10. Outer Bed Loadings of FBTE 5 (also FBTEs 1 and 2).

Fig. 11. Outer Bed Loadings of Fuel Test Elements. For FTEs 1 and 2 see Fig. 8; loading of all three bodies in each element is identical; all bonded rods.

Fig. 12. Design of Thermal Stability Spine Samples.

Fig. 13. Design of Fission Product Release Spine Samples.

Fig. 14. Design of Alternative Fission Product Release Spine Samples.

Fig. 15. Design of Diffusion Spine Samples.

Fig. 16. Design of Graphite Transient Creep Spine Samples.

DISCUSSION

S. I. Kaplan: Please describe the removal of the broken fuel element this past month.

John Kemper: During the removal of the Peach Bottom Core I fuel elements that had developed cracked graphite sleeves, one of the fuel elements fell back into the core. When a second attempt was made to remove the fallen element only the upper section could be removed. A special removal tool and jacking device were developed and tested. The bottom section was jammed on the standoff pin, but with the new tool it was successfully removed without difficulty. As of this date, all of the fuel elements that had experienced cracked sleeves have been successfully removed, and over 200 new Core II type elements have been loaded. It is planned to have Core II loaded and the plant returned to power by early this summer. It is a credit to the refueling team and the HTGR concept that with considerable unforeseen fuel element removal problems, the elements were all removed from the core in a relatively short period of elapsed time.

G. Meijer: Referring to your irradiation experiments of 1100 Mwe HTGR fuel, what are the design burnup, fluence and temperature conditions for the 1100 Mwe HTGR fuel?

K. P. Steward: Maximum burnup in mixed fissile particles is 20%. Maximum burnup in UC_2 buffer diluted fissile particles is approximately 75% maximum; fast fluence expected is approximately 8×10^{21} nvt. Maximum temperatures experienced by the fuel will be 1350°C.

D. Tytgat: 1. What will be the different pyrocarbon densities adopted for the coated particles? 2. The fuel element experiments in Peach Bottom are supposed to stay 1, 2, and 3 years. Are these years full power operating years or calendar years?

K. P. Steward: 1. On BISO particles the "high" density PyC will be 1.80-1.90 g/cm³ and the "low" density will be 1.55-1.65 g/cm³. Outer PyC densities on TRISO particles will be 1.70-1.80 g/cm³. 2. The operating periods referred to are increments of 300 effective full power days. Each

such increment will take up approximately one calendar year of Peach Bottom irradiation.

Hugh B. Stewart: John Kemper has pointed out the effective way in which the cracked Core No. I fuel elements have been removed. It is worth noting that the cracked sleeves in Core No. I occurred as a result of compact expansion due to the very primitive coated particles that were used in that core. In fact, Roy Huddle has pointed out that these particles were not coated particles; they were "covered" particles. Core No. II will use the highly developed coated particles now available to us and the phenomenon that was observed in Core No. I will not occur in Core No. II.

PLANT CONSTRUCTION EXPERIENCE

(Session II)

Chairman: G. E. Lockett,
OECD High Temperature Reactor Project

Co-Chairman: M. Bender,
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FORT ST. VRAIN CONSTRUCTION PROGRESS

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ABSTRACT

The 330-Mw(e) Fort St. Vrain Nuclear Generating Station is being built by Gulf General Atomic Incorporated as prime contractor for the Public Service Company of Colorado as part of the USAEC Power Reactor Demonstration Program. Among the significant design features incorporated in the plant are the prestressed concrete reactor vessel, once-through modular steam generators with integral superheaters and reheaters, steam-driven axial-flow helium circulators, and hexagonal graphite fuel elements incorporating improved coated fuel particles. Construction of the plant featured the use of ground freezing to provide a cofferdam to keep ground water from the excavation and preplaced aggregate concrete for the bottom head of the prestressed concrete reactor vessel. As of March 31, 1970, construction is estimated to be over 62% complete and on schedule for a completion date of June 1971.

INTRODUCTION

The 330-Mw(e) Fort St. Vrain Nuclear Generating Station, now under construction, is being built by Gulf General Atomic Incorporated for the Public Service Company of Colorado and in cooperation with the USAEC as part of the Power Reactor Demonstration Program. The station is located approximately 35 miles north of Denver, Colorado, and is scheduled to go into commercial operation in 1972. Gulf General Atomic has retained Sargent & Lundy as Architect-Engineer and EBASCO Services as constructor.

The Fort St. Vrain plant incorporates a number of significant design features new to power reactor systems. The most prominent are the prestressed concrete reactor vessel (PCRVR), once-through modular steam generators with integral superheaters and reheaters, steam-driven axial-flow helium circulators,

and hexagonal graphite fuel elements incorporating improved pyrocarbon-silicon carbide coated fuel particles.

The flow diagram shown in Fig. 1 points out the major operating parameters. The helium coolant at a pressure of about 700 psia flows downward through the reactor core where it is heated to 1430°F. The coolant flow can be trimmed by the use of orifice valves located at the top of the core that are integral with the control rod drive mechanisms. From the reactor core, the coolant flows through the steam generators. After passing through the steam generators, the helium is returned to the core at a temperature of about 760°F by four steam-turbine-driven helium circulators. Two identical loops are used, each including a six-module steam generator and two helium circulators. Each loop contributes half the total output of the nuclear steam supply system, which produces steam at 2400 psig and 1000°F with single reheat to 1000°F. The helium circulators are driven by the exhaust steam from the high-pressure turbine. This steam is then reheated and returned to the intermediate-pressure turbine. The circulators are also equipped with a Pelton water wheel drive so that they may be driven using the boiler feed pumps for emergency conditions.

The general reactor arrangement can be seen in Fig. 2. The prestressed concrete reactor vessel (PCRV) is 31 ft in internal diameter with a 75-ft internal height. The upper and lower heads are nominally 15 ft thick, and the walls have a nominal thickness of 9 ft. Thus the PCRV provides the dual function of containing the coolant at operating pressure and also providing radiological shielding. The exterior vertical surface of the vessel may be described as an hexagonal prism with vertical pilasters at each corner. It is 61 ft across pilasters, 49 ft across flats, and 106 ft high.

The concrete walls and heads of the PCRV are constructed around a carbon-steel liner which is nominally 3/4 in. thick. The liner is anchored to the concrete and provides a helium-tight membrane. A system of water-cooled tubes welded to the concrete side of the liner provides a heat-removal system to control concrete temperature. In addition to the coolant tubes, a thermal barrier is provided on the inside surface of the liner to limit the flow of heat to the liner walls.

The top head has refueling penetrations that also house the control rod drives. It also incorporates penetrations and wells to house the helium purification system and neutron detection chambers. The bottom head has penetrations for each of the steam-generator modules and helium circulators plus a large central opening for access to the main cavity.

All of the penetrations through the PCRV are provided with two independent closures. The PCRV inner cavity and the primary closures serve as primary containment for the reactor; the massive PCRV and the secondary closures act as the secondary containment.

The vessel is prestressed to place the concrete structure in compression prior to service. The prestressing system used is known as a linear tendon posttensioning system. During construction, steel tubes are embedded in the vessel concrete for later insertion of the tendons, each of which consists of up to 170 1/4-in.-diameter "thermalized" wires. The wires are anchored by means of cold-deformed button heads to a washer assembly that transfers and distributes the loads over split shims and a steel bearing plate into the concrete. Three tendon arrangements are used: 90 longitudinal (vertical) tendons, 310 circumferential tendons, and 48 crosshead tendons.

Inside the PCRV is the core support floor, a water-cooled structure of steel and reinforced concrete 5 ft thick. It is supported from the bottom of the PCRV cavity by 12 water-cooled steel columns. Twelve ducts conduct reactor-outlet helium through the core support floor to the 12 steam generator modules located below.

CONSTRUCTION

One of the first things done after moving onto the construction site in April of 1968 was to pour a concrete pad adjacent to the area where the reactor building would be located. This provided a working area for the erection of the PCRV liner bottom head and other liner components, thus allowing work to be carried out concurrently with the construction of the reactor

building foundation and the support ring for the reactor vessel. Figure 3 shows the liner assembly area and the reactor building excavation. By carrying out the PCRV liner fabrication concurrently with the construction of the reactor building foundation, the schedule was shortened significantly. Concurrently with these two operations a plywood mockup of a section of the PCRV bottom head was constructed for use in training the construction force and to help in planning future operations.

Construction of the reactor building required an excavation to bedrock, which was located some 50 to 55 ft below ground level. The excavation was to be roughly 100 ft x 150 ft x 50 ft deep with additional penetration into bedrock of 25 ft in some areas. This excavation required the removal of about 30,000 cu yd of sand and gravel to expose the Pierre shale bedrock and somewhat over 3,200 cu yd of shale. Excavation work was complicated by the fact that the water table was at a depth of 20 to 25 ft in sands having a high transmissability and was further complicated by the characteristic of the Pierre shale to decompose upon exposure to air. For normal excavation of this type of sandy soil, side slopes would have had to be at least 2-1/2 to 1. This would have increased the yardage to be excavated and would also have caused a large portion of the area for the turbine building to be unavailable for work as long as the excavation was open.

Although sheet piling and conventional well point dewatering was considered, the underground water flow was so strong that it was questionable whether this method could have successfully kept the excavation dry. The ground freezing technique was selected as a better method and because it enabled nearly vertical side excavation and provided a wall capable of supporting heavy loads, thus permitting close access to the work.

The freeze-wall method of providing a cofferdam, shown in Fig. 4, is relatively new in this country. For an oblong excavation such as Fort St. Vrain, the wall, in plan view, is roughly an ellipse. The thickness of the frozen wall averaged about 6 ft at the top, and at rock elevation, about 8 ft. (Four locations were deliberately thickened to serve as crane platforms.)

To construct this wall, 180 U-tube pipe assemblies consisting of two 55-ft sections of 1-1/2-in. pipe connected by a return bend at the bottom, were sunk into the ground on the perimeter in predrilled 4-in. holes filled with a bentonite slurry as a drilling aid. Ninety more assemblies were sunk on the inner perimeter, and an additional 90 assemblies were used for crane pads and gantry track pads. All of the U-tubes were connected to common brine supply and return headers, which ran from the refrigeration compressor house near the northeast corner of the excavation. After the wall was frozen, the brine (with a freezing point of -45°F) was continuously circulated through all of the pipes at a supply temperature of -11°F and a return temperature of -6°F . The refrigeration system consisted of two 85-ton and one 66-ton single-cycle ammonia compressors. Each refrigeration unit had its own cooling tower. In the event of a power failure, a standby diesel generator of 625 kVA capacity was connected to the system.

Wall freezing was started on May 6, 1968, and by June 3, 1968, most of the wall was frozen. In order to expedite closure in some areas, additional U-tubes were installed, and liquid nitrogen was introduced. In other areas that proved difficult to close, additional holes were drilled and a clay slurry was introduced to impede the flow and permit freezing to occur.

To protect the shale-type bedrock from deterioration by exposure to air, the shale had to be covered with concrete or gunite within eight hours. It was impossible to protect the shale and to line drill and blast simultaneously. As a result the shale was excavated only to rough limits, leaving about 9 to 12 in. of shale in place. The shale was then cut back to finish limits and protected with gunite on the vertical surfaces and concrete on the horizontal surfaces.

The excavation was completed in time to permit the first permanent concrete to be poured on September 18, 1968, following the award of the construction permit on September 17, 1968. The freeze-wall system remained in operation until the reactor building basement walls were completed and backfilled to grade in February 1969.

The support ring for the PCRV, which is a cylinder 50 ft in outside diameter, 3 ft 6 in. thick, and 33 ft high, required 575 cu yd of concrete. When all the steel was complete, the concrete was placed in a continuous pour over a period of 18 hours using Pumpcrete machines. The final seismic design for the ring required a much larger quantity of reinforcing steel (see Fig. 5) than was originally anticipated. Because of this, a 14-week delay occurred in the completion of the ring, which took place on February 13, 1969.

Meanwhile, work on the bottom head assembly consisting of the steel liner, penetrations, and the core support floor columns, which was started in June 1968, was completed, and the assembly was moved to its permanent position over the PCRV support ring in February 1969. This assembly, which weighed some 400 tons including a temporary erection jig, steel forms for the bottom head, and some of the reinforcing steel, was moved on rails by a modified turbine generator stator lifting rig and was lowered into place on temporary support steel erected within the support ring. Figure 6 shows the assembly during the move.

The construction sequence required that the bottom head liner plate be in place prior to concrete placement, in order to assure intimate contact between the concrete and the underside of the bottom head liner and penetrations. It was therefore decided to use the Preplaced Aggregate Concrete (PAC) technique in the bottom head to ensure satisfactory quality. As the name implies, the PAC technique consists of placing clean coarse aggregate into the forms. The aggregate is placed in layers to avoid formation of large voids in the aggregate mass, and to achieve intimate contact with all embedments. Grout is pumped under pressure in a continuous operation through previously embedded pipes to fill the voids existing in the aggregate mass. For the Fort St. Vrain PAC, the voids amounted to about 48 per cent of the aggregate volume.

A full-scale mockup of a section of the bottom head was made using PAC to investigate the concrete strength properties as well as other variables of PAC and to test the capability of PAC grout pumping equipment, the capabili-

of the batch plant to mix the grout within the stringent limitations of the specifications, and the capability of monitoring equipment to be used for grout level indication, and the ability to place the PAC without voids. The results of the mockup were highly encouraging and provided information that was valuable in operational planning for the PAC for the bottom head.

After all the bottom head embedments, including tendon tubes and reinforcing steel were installed (Fig. 7), 1400 cu yd of 1-1/2-in. aggregate were placed into the bottom head forms. The aggregate was screened and washed before being loaded on trucks for delivery to the PCRVR. At the delivery point, it was again passed through a rotary washer and discharged into a holding hopper. From the holding hopper the aggregate was placed into position by hand (Fig. 8).

Before the grouting operation was started, the aggregate was cooled by flushing and partly filling the forms with cold water and ice. The grout ingredients, sand, fly ash, cement, intrusion aid (a combination of fluidifier and expansion agent), water, and ice were batched and measured by weight $\pm 1\%$ by a Gulf General Atomic automatic concrete batching plant, which is located on the site and provides all of the job concrete. The plant is rated at 100 cu yd/hr (with provisions for heating or cooling the concentric mix) and is capable of handling all required design mixes. A well-equipped testing laboratory, which includes a 600,000-lb Forney testing machine, permits testing of all concrete and reinforcing steel and includes the capability of testing to failure a size 18S bar.

The batches were loaded into concrete transit mixing trucks that premixed the grout. A mixing and pumping plant, consisting of three stationary mixer-agitator units and 12 pumps, was erected at the PCRVR site. The mixer-agitator discharged the grout into a common manifold to which the 12 horizontal piston pumps were connected. Only two of the mixer-agitators and eight pumps were actually in use during the bottom head grouting operation, the remaining equipment being on standby. The mixer-agitators and the manifold were provided with cooling coils and insulation to maintain the grout at $45^{\circ} \pm 3^{\circ}\text{F}$. Prior

to release of the grout from the mixer-agitators into the manifold, the temperature and fluidity of the grout were tested. Each pump served one insertion point at a time. The insertion points were changed as the grouting operation proceeded.

Grout flowed under pump pressure through the grout pipes into the aggregate. Horizontal 1-in.-diameter grout pipes were placed in several horizontal layers. The number and location of the horizontal grout pipes were sufficiently redundant to provide full and uniform grout coverage of the PCRV bottom head.

Grout injection started from the lowest point in the form at the center of the aggregate mass. As the grouting operation proceeded the grout tubes were slowly retracted to produce an upward and outward grout flow from the center to the periphery. The slope of the advancing grout was such that entrapment of air was prevented, and displacement of laitance and water toward the venting areas was achieved.

Short vertical grout pipes were employed for topping off at the construction joint. A surplus of grout was injected to more than fill the aggregate mass until overflow of undiluted grout was observed at the surface of the aggregate. Diluted grout was continually broomed off the surface. The grouting operation took 54 hours and was completed on June 21, 1969.

The use of sounding or observation wells, which are usually employed for grout-level detection and control in preplaced aggregate concrete work, was not feasible for the PAC in the PCRV because of the requirement that no openings were allowed through the bottom liner plate. An electronic method for determining grout levels was therefore developed, using the principle of time domain reflectometry. The principle is based upon sending a fast-rise pulse down a cable and observing the amplitude characteristics of the reflected pulse. As the transmitted pulse encounters discontinuities in the cable, marked changes in amplitude are detected. One of the cable discontinuities that can be detected is the point in the cable where the surrounding medium changes from air to water or grout. The signal was displayed on an oscilloscope.

There were 94 pairs of sensor wires, each wire running the full depth of the aggregate mass. The complete profile of the grout at any time during the grouting operation could thus be observed and displayed in a three-dimensional plastic model. The continuous visual display in the model of the grout levels allowed a controlled grouting operation. After completion of the grouting operation, just below where the initial set of the grout was expected, the wire mesh on the construction joint surface was removed in preparation for the "green cutting" operation, which was started after the initial set but prior to a full hardening of the grout. Green cutting removed the surface grout and exposed the aggregate without loosening or breaking the aggregate particles. The construction joint surface was wet-cured thereafter.

The large mass of the bottom head PAC, approximately 1600 cu yd, required careful temperature control during the hydration phase. One hundred and forty thermocouples measured the concrete temperatures. The cooling system was designed to maintain concrete curing temperatures below 130°F. The actual measured maximum temperature was 117°F. Cooling was provided by (1) circulating refrigerated water through 70 selected vertical tendon tubes and through supplemental pipes (2 in. diameter) embedded in the concrete, (2) circulating refrigerated water into the bottom head liner penetrations, and (3) maintaining the ambient temperature of the surfaces to about 60°F by a system of cool water sprays and wet burlap. A 190-ton cooling system provided the necessary refrigerated water. Postcooling was commenced at the completion of grouting and was terminated when the average concrete temperature was down to 70° ± 10°F.

The remainder of the concrete for the PCRV was conventionally mixed and placed. The concrete, which is designed for a strength of 6000 psi, contains aggregate that is carefully controlled for gradation, friable particles, and organic content. The concrete temperature is limited to 130°F during curing to minimize shrinkage and high-temperature-gradient cracks. An extensive inspection and testing program was developed for the vessel concrete to ensure compliance with the specification.

In preparation for pouring the sidewall concrete lifts, the liner barrel sections were completed in the assembly area, moved into the reactor building

(Fig. 9), and welded in place. Rebar, tendon tubes, and forms were placed for the 14 successive lifts of about 5 ft each making up the PCRV sidewall. The first lift was poured on July 17, 1969, with the fourteenth on January 7, 1970. In the meantime, the top head liner has been completed and installed (Fig. 10), and preparations were under way for the three lifts of the top head. The first of these lifts was poured on January 30, and the final pour to complete the PCRV was made on February 13, one week ahead of schedule (Fig. 11). This is particularly impressive because it includes 14 weeks lost on the schedule during the construction of the support ring. The total concrete placed in the PCRV is about 6500 cu yd, including the 1600-cu yd PAC in the bottom head.

The installation of the tendons was started in October 1969. The tendons are partially fabricated when they are received at the site. After they are pulled through the in-place tendon tubes, they are buttonheaded and then pre-stressed with 1000-ton jacks from both ends of curved tendons and one end of the straight tendons (Fig. 12). The first tendon was stressed on January 16, 1970. As of March 31, all 448 tendons in the PCRV have been installed, and 142 have been stressed.

As the concrete was being placed for the PCRV sidewalls and top head, workers continued with the PCRV internals. The internals include the liner insulation, support columns for the core support floor, the core support floor, and core barrel. Steam generator installation was started in January 1970. This operation was begun by installing the secondary modules, which reside within the penetrations in the PCRV bottom head (Fig. 13).

Installation of the helium circulators and the fuel transfer machine is scheduled to begin in July - August of this year. The conventional part of the plant is progressing at an equally rapid pace and is on schedule.

Since moving onto the construction site in April 1968, over 50,000 cu yd of earth have been excavated, nearly 38,000 cu yd of concrete have been poured, and over 3,700 tons of steel have been erected or installed. As of March 31, 1970, the construction is estimated to be over 62% complete and on schedule. Figure 14 is a recent over-all view of the construction site.

Construction is on schedule for a completion date of June 1971.

Fig. 1. Simplified flow diagram.

Fig. 2. General arrangement of the primary reactor components.

Fig. 3. View of construction site showing liner assembly area to the left of reactor building excavation.

Fig. 4. Plan view of freeze-wall cofferdam.

Fig. 5. Top of support ring rebar structure showing PCR support hinge mounting corbels.

Fig. 6. PCRV bottom head assembly (approximately 400 tons) being moved to position on temporary support steel inside the support ring (covered with plastic).

Fig. 7. PCRV bottom head reinforcing steel and tendon tubes.

Fig. 8. Special aggregate being preplaced in the bottom head prior to the pumping of the grout.

Fig. 9. Liner sections two and three being lowered into position on top of the first liner section. This double section is 34 ft high.

Fig. 10. PCRV liner top head in its permanent location prior to the pouring of the 14th and final sidewall lift.

Fig. 11. Final concrete placement in the PCRV top head.

Fig. 12. 1000-ton jack in place for stressing a circumferential tendon.

Fig. 13. One of the secondary steam generator modules being moved into position for installation in a bottom head penetration.

Fig. 14. Over-all view of construction site showing switch-yard (right foreground), reactor building - turbine building (center), and main cooling tower (left rear).

DISCUSSION

D. B. Trauger: Future reactors are of the pod design which is a more complex configuration. How does the Fort St. Vrain vessel construction experience extrapolate to the future vessels, particularly with placement of concrete and prestressing sequence? Will they be more difficult or easier?

H. N. Wellhouser: We believe the pod design is simpler to construct. There are many fewer tendons to stress. The concrete placement experience gained with Fort St. Vrain is directly relateable. The concrete placement requirements for the 1100 Mwe vessel should be considerably easier than Fort St. Vrain because of the absence of cross head and circumferential tendons.

G. Meijer: Can you give the construction time being estimated for the 1100 Mwe HTGR?

H. N. Wellhouser: Actual construction time for the nuclear steam supply system is estimated to be 37 months.

G. E. Lockett: I understood you to say that the maximum lift was about 190 tons and this was done by a 160 ton crane. I should be interested to know how this was achieved.

H. N. Wellhouser: In the U.S., crane manufacturers allow for a 25% overload provided the overload lifts are done infrequently. In this case we worked closely with the manufacturer so as not to void the warranty.

M. Bender: In the past, there have been claims that concrete vessels would shorten the construction schedule for a reactor plant over that required with steel vessels because of the delivery period needed for steel vessels. Could you comment on the validity of that claim on the basis of Fort St. Vrain experience? Could a concrete vessel be constructed in the equivalent period of time needed for a concrete containment vessel?

H. N. Wellhouser: It seems the construction time for the PCRV is somewhat shorter than for vessels common to water reactors. With the

exception of the reactor internals, the Fort St. Vrain PCRV could be completed and stressed in about 24 months.

B. G. Chapman: What techniques have been used for providing the leak tightness of the PCRV liner?

H. N. Wellhouser: All welds on the PCRV liner are 100% radiographed and helium leak checked.

G. D. Whitman: What is the status of the prestressing operation?

H. N. Wellhouser: All 448 tendons have been installed and stressed. The last tendons were stressed on April 24, 1970.

T. A. Jaeger: Has the design of the prestressed concrete reactor pressure vessel been developed on the basis of the conventional standard specifications for prestressed concrete or did you apply specifically developed design criteria?

Concerning the utilization of the ground breathing technique, I would like to know whether a detailed description of the experiences, especially of the troubles experienced, is available.

H. N. Wellhouser: Concrete standards were used where applicable as was ASME Section III nuclear code.

I don't know of any published report that covers this work in the detail I think you are looking for.

THE RELATIONSHIP OF THE HTR WITH EARLIER GAS-COOLED REACTORS

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ABSTRACT

Many of the potential advantages of the HTR stem from the concept of particle fuel and the use of helium as a coolant. It is therefore significantly different in several aspects from the type of reactor already developed in the UK, but it is still basically a gas-cooled reactor system. The design and operating experience gained with the magnox reactors and the AGR, even if not immediately applicable, is directly relevant to determining the type of problems likely to be encountered and to finding the economic solutions. This paper reviews the design, experimental and constructional experience of early gas-cooled reactors which is applicable to the HTR.

INTRODUCTION

A nuclear power station is a large and technically complicated investment made with the single object of producing electricity economically. Innovations are required to reduce both the capital cost and the running cost of new power stations, but must not be introduced at the expense of reliability. An investment will only pay for itself if a high load factor can be achieved. One way of achieving successful innovation with continued reliability is to develop a family of reactors, each drawing on the experience of its forbears.

In the UK the gas-cooled reactor has been developed over 20 years. The initial decision to adopt gas cooling and graphite moderation was taken for economic reasons relating to the availability of diffusion plant and heavy water. The early magnox reactors have now been operating successfully for a number of years with high load factors and, despite their high initial capital cost, have met their target generation costs.

The High Temperature Reactor (HTR) is the latest development in the design of gas-cooled reactors. In the US and in Germany development and power station construction are going ahead rapidly. In the UK the extensive experience of the design and construction of gas-cooled reactors is available to take maximum advantage of the potential of the HTR. This paper reviews some of the relevant experience and indicates how the design is being evolved.

COOLANT GAS

General Properties

The coolant gas is the heat transfer and heat transport medium within the reactor system. The overall properties of helium are little different from those of carbon dioxide in this respect. The low density is offset by the high specific heat so that flow velocities are not greatly dissimilar, while dynamic forces are decreased. In local areas the particular properties of helium lead to noticeable differences. For example, the high thermal conductivity of helium requires a thickness of insulation pack up to three times that in carbon dioxide. In general, however, because the properties are so well known, the effects can be calculated and the experience with carbon dioxide can be readily utilised.

The chemical properties of helium are markedly different from those of carbon dioxide, the oxygen content of which has led to corrosion problems with low grade mild steels. Helium is chemically inert, for practical purposes, in a reactor circuit, but the initial experiences at Dragon and Peach Bottom have suggested the increased importance of trace impurities. The circuit gas will have a hydrogen level of the order of 10 ppm, because of the chemical reduction of water from minute leaks from the boilers and from diffusion of hydrogen through the boiler tubes.

In the resultant reducing atmosphere the metal components of the circuit may lose their normal protective oxide layer, leaving them open to attack from further impurities. In parallel, the surface properties of the materials will be different to those in an oxidising atmosphere, and friction and wear phenomena must be carefully investigated.

Chemical Properties

A nuclear power station has to be built necessarily for at least a 20 year life. The strength, and particularly the creep performance, of most common engineering materials are known for such long operating periods, or can at least be confidently predicted. The effect of the environment within a nuclear reactor is uncertain however, so during the early design period of the magnox reactors extensive experimental programmes were initiated. Of particular interest was the chemical reaction between the coolant, carbon dioxide, and the materials of those components in the reactor, which because of radiation effects could only be replaced with great difficulty.

There was a known reaction between the gas and the graphite moderator, the reaction rate being dependent on both the temperature and the neutron flux. This reaction was investigated using complex in-pile rigs and the design information produced has been entirely satisfactory, as shown by the successful operation of the Calder reactors for 15 years. The reactions with other materials were also investigated and similar design information was produced. Unfortunately the effect of break-away corrosion in low silicon semi-killed steels was not fully appreciated. Of little concern on open surfaces, it can produce strains in mechanically restrained assemblies and this was not noticed until inspection of the Bradwell reactors after they had been running successfully for 6 years. It has been found necessary for the time being to reduce gas temperatures at Bradwell, and all succeeding magnox stations, to control the rate of oxidation, and inevitably the output has been reduced. Nevertheless a load factor (based on guaranteed output) of 82.3% was achieved at Bradwell during 1969 despite the restriction.

The research programme on steels has been increased since the discovery of the strain phenomena, particularly to cover the higher pressure and temperature conditions of the AGR. In general these reactors make much more use of stainless steels, which have a higher resistance to oxidation and no difficulties are expected. Research is continuing, however, to confirm existing results.

The chemical properties of helium are entirely different to those of carbon dioxide, but similar research is investigating any corrosion effects of impurities carried by the gas. A new phenomenon is the effect of the reactor gas on the surface properties of materials, particularly the coefficient of friction. Table 1 gives some typical results of dynamic friction coefficients measured in pure helium.

Table 1. Dynamic Friction Factors

Material Combination	Helium Pressure Atmospheres	Temperature °C	Contact Pressure kg/cm ²	Friction factor		
				min	max	mean
Cr Carbide on self	1	800	65	0.62	1.29	1.17
	50	800	65	0.37	0.56	0.47
Graphite on self	1	800	65	0.24	0.41	0.34
	50	800	65	0.22	0.34	0.30
316 Stainless steel on self	1	650	21	1.85	2.83	2.65
	50	650	21	1.08	2.92	1.6

These results are very different from the corresponding properties in air and show an interesting tendency for the coefficient to decrease with increasing gas pressure. The absolute levels are high however, and emphasise the care which will have to be taken in material selection.

Vibrations and Noise

Of the order of 5% of the gross electrical power produced in a gas-cooled nuclear reactor station is used to circulate the gas around the system. There is therefore a considerable amount of energy in the gas stream which, in the event of a vortex shedding frequency coinciding with a natural frequency within a contained volume, could cause significant mechanical damage. Several effects such as this have been reported during the history of gas-cooled reactors¹ and illustrate the growing sophistication in commissioning experimentation required as gas pressures and flow dynamic heads increased with reactor development.

At Dungeness 'A' early difficulties were experienced during the preliminary commissioning tests of a bypass circuit. This circuit, which

is 60 ft long and 4 ft diameter bypasses the circulator outlet gas back to a point mid-way along the length of the boilers adjacent to the evaporator bank of tubes and enables the circulators to be run independently of the main reactor gas circuit. During the circulator commissioning it was found that at speeds above a critical value the noise level and duct vibration became intolerable. The circuit was instrumentated with strain gauges, accelerometers and pressure transducers and tests showed that the frequencies at which the duct was vibrating were 133 Hz and 158 Hz. The maximum stress level in the ducting was measured at 780 lb/in.² peak-to-peak; at 133 Hz the maximum acceleration recorded was 9 g and the maximum gas pressure pulsation was 0.7 lb/in.² peak-to-peak.

After considerable investigation, which included measuring the predominant response frequencies of the duct, the phenomenon was traced to resonance between an aerial vibration in the duct and a vortex shedding frequency from the trailing edges of two cruciforms supporting the tongue of a duct bellows unit. The severe vibrations were eliminated by modifying the cruciform trailing edges, when the maximum duct acceleration level was reduced to 0.64 g.

A similar and potentially more serious resonance was found during boiler commissioning between an aerial vibration in the boiler shell and part of the actual tube assembly, i.e. the top rows of the superheater and the support beam system. In this case it was not possible to modify the initiating member, so an attempt was made to fit baffles within the boiler shell to change the frequency of the aerial vibration.

Tests at site became so time-consuming and expensive that it was decided to adopt wind tunnel techniques to find the form of a suitable baffle, within the confines of the existing tube banks. A considerable amount of modelling work was necessary before conditions in the boiler could be reproduced, but eventually several forms of solution were evolved which, in the wind tunnel, suppressed the resonance. Not all of these solutions were successful in the actual boiler however, for reasons which are not fully understood, although it may have been either an over simplification in the modelling technique or a difference in

some unappreciated critical dimension between the model and the final application. The final solution of fitting transverse baffles immediately behind some of the tube banks was successful, however, in reducing the maximum alternating stress measured on a tube to $\pm 0.4 \text{ t/in.}^2$, which is significantly less than the fatigue strength assessed on the life of the plant.

During the design stage for Oldbury an analysis was made of the possible vibrations of the many components. Calculations showed that the boilers could be susceptible to effects similar to those found at Dungeness 'A', so that design was changed to incorporate a continuous vertical baffle through the tube banks. As a precaution, commissioning tests were carried out first with cold air at atmospheric pressure and then with hot pressurised air at a range of conditions chosen to give the required sonic velocities and gas density. In the event, the stress level within the boilers was found to be insignificant, although a discrete pressure pulse was present throughout the entire circulator speed range at a frequency of within 5% of that predicted.

Oldbury was indeed a very 'quiet' reactor in terms of vibrations but during the commissioning a new phenomenon related to the noise produced by the circulator was found. This was a considerable amount of sound energy appearing at very low frequencies (of the order of 50 Hz) in areas very close to the circulator outlet and representing, therefore, a potential hazard to the insulation cover plates.

For the AGR design at Hinkley 'B' the design of the cover plates has been modified in case similar effects occur. The circulator drive is a constant speed electric motor, and the impellor and diffuser have been designed to avoid all significant trace of blade-passing frequency. The preliminary tests at atmospheric pressure have indicated low over-all noise levels, but a full test programme is planned to investigate the final levels in the reactor.

To complement the AGR commissioning tests, extensive experimental facilities are being developed by TNPG. These will be used also to check the predictions on noise performance with helium circulators. No

immediate problems are apparent, for although the velocity of sound is very much higher the acoustic impedance (sound velocity x density) is little different to that of carbon dioxide. Noise, as a general subject, has only a limited theoretical basis however, especially in enclosures where the dimensions are less than the wave-length of the sound waves, and a considerable amount of experimental work will be necessary.

CIRCUIT ENGINEERING

Definition

The term circuit engineering in a gas cooled reactor includes all the plant which is necessary to translate the heat generated in the reactor into steam to drive the turbo-generators, i.e. the reactor pressure vessel and its associated liner, insulation and cooling equipment, the boilers, and the gas circulators.

The pre-stressed concrete pressure vessel is the basis of the circuit engineering of an integrated concept giving the designer freedom in specifying vessel shape and gas pressure and yet always guaranteeing reactor safety because of the massive redundancy in the number of stressing cables. The concrete must be isolated from the high reactor gas temperatures, however, and one of the major problems which has arisen is the design and erection of the liner insulation.

Insulation

Metallic foil and wire mesh insulation and fibre insulation are the two main types which have been used in the concrete pressure vessels built in the UK. TNPG have been heavily involved in the development work for both types.

Metallic Foil and Wire Mesh Insulation

Feasibility studies to prove the integral concrete pressure vessel concept for the Magnox reactors were completed by TNPG in 1960 and the insulation of the vessel liner was then realised to be a major problem. There was virtually no published information relevant to the thermal performance of insulants in a high pressure gas environment and most conventional insulants were excluded because of the compatibility requirements.

Development work was therefore started by TNPG. Subsequently it was learned that Darchem Engineering Ltd were working on parallel lines and from 1962 with the Oldbury tender awarded to TNPG, the two research and design teams worked together to develop what is now called metallic foil and wire mesh insulation. As an example of this material the Oldbury reactor walls^{2,3} were insulated with 30 layers of 0.004 in. stainless steel foil separated by a wire mesh (0.048 in. dia x 2 in. pitch), the whole pack being compressed by a mild steel cover plate $\frac{1}{4}$ in. thick to a pack thickness of 2.00 in.

Similar insulation has since been used for the Wylfa (Mgnox) and for Dungeness 'B' (AGR) reactors. Considerable commissioning and operational experience has therefore been obtained in carbon dioxide at temperatures up to 400°C and pressures up to 400 psia. The major problems associated with surface pressure gradient effects have been fully reported^{2,3}.

Fibre Insulation

With the completion of the Oldbury insulation, work on all aspects of the AGR insulation was accelerated. The compatibility requirements of the AGR were less severe than for Mgnox reactors and a much wider range of materials could therefore be used. Higher temperatures and pressures (650 psia and 650°C for Hinkley 'B') had to be considered. TNPG decided to review the whole field in an attempt to develop an insulant to meet the following specification:

- (a) The required thermal performance under natural convection over the full range of pressures and temperatures likely to be used in AGRs and HTRs.
- (b) A high resistance to surface pressure gradients.
- (c) A low installed cost. This involves both low initial cost and a relatively small site cost. Installation time was to be kept to a minimum.
- (d) The insulant must be capable of accommodating local irregularities of the cold face (reactor liner).

- (e) Isotropic properties. Insulants with highly anisotropic properties cannot readily be used for penetrations and regions with complex boundaries.

As a result of the review it was clear that a fibrous insulation had many of the required properties and this type of insulation was eventually chosen for Hinkley 'B' and Hunterston 'B'. The development work for both these stations is being carried out in conjunction with the insulation contractors, Delaney Gallay Ltd. As an example, the insulation for the liner walls consists of layers of Triton Kaowool fibre compressed by a $\frac{1}{4}$ in. thick stainless steel cover plate to 2 in. thickness overall. The cover plate is mounted on a single bimetallic stud welded to the liner.

Thermal Performance

In the last ten years work in England, France and elsewhere^{4,5,6,7} has enabled an adequate understanding of the thermal performance of insulants in a high pressure gas environment to be established. The bulk of the experimental work has been carried out in high pressure carbon dioxide but the limited work in helium^{8,9} is in fair agreement with the correlations developed.

Metallic Foil and Wire Mesh - Natural Convection. — In any analysis of experimental data the convective contribution is first obtained by deducting the metal conduction and radiation terms¹⁰ from the measured conductivity of the insulant. The convective contribution, which in most realistic assemblies in carbon dioxide is the dominant contribution, can then be correlated for a given insulant assembly against the pack Rayleigh number (Fig 1).

where

$$\begin{aligned}
 N_u &= \text{Nusselt number} \\
 &= \frac{\text{heat out of cold face when gas movement occurs}}{\text{heat out of cold face under static gas conditions}} \\
 \overline{N_u} &= \text{Mean Nusselt number over cold face} \\
 R_a &= \text{Rayleigh number} = \frac{C_p \rho_m^2 d^3 g \Delta T}{\mu_m k_g n T_m}
 \end{aligned}$$

For values of R_a above 100 convection within the insulation becomes

significant. Two lines are shown on Fig 1 which indicate the improvements that are possible as a result of internal sealing features. The lower line can be considered typical of the performance of well designed insulation incorporating back lap seals^{2,3,4} to reduce the permeability in the plane of the foils.

As a guide to thermal performance, complex flow pressure drop measurements are carried out on representative insulation details. These measurements have shown that foil and wire mesh is highly anisotropic; the permeability measured in the plane of the foils being between 10^4 and 10^5 times that measured normal to the foils.

The most successful theoretical model, developed by Davidson⁴, treats the whole pack as an anisotropic permeable solid.

Fibres and Granular Materials - Natural Convection. — For isotropic materials the Davidson theory is equally applicable⁶ and a comparison between predicted and measured mean Nusselt number is given in Fig 2. Data from⁵ are also included.

where

$$R_a = \text{Rayleigh number} = \frac{C_p \rho_m^2 \beta_y D g \Delta T}{\mu_m K T_m}$$

β_y = Permeability in the vertical (y) direction

D = Pack thickness

H = Pack height

K = Thermal conductivity of a porous body with no overall gas movement through the body.

Otherwise the same notation has been used as defined on Fig 1.

The importance of one parameter which is normally overlooked, namely the sealing characteristics of the hot face, is clearly demonstrated. This correlation is equally applicable to HTR conditions where it should be possible to reduce the value of $R_a D/H$ to below the value for which convection and hot face sealing effects are important.

Surface Pressure Gradient Effects. — In gas cooled reactors it is inevitable that the hot face of the insulation is exposed to surface pressure gradients. The insulation must be capable of satisfactory

operation when rapid pressure changes (due for example to a change in reactor power) take place and to achieve acceptable pressure differentials across the hot face it must be permeable. Surface pressure gradients can therefore influence the pressure distribution within the insulant.

Davidson's theory has been extended to cover this case⁶ and the influence of a uniform pressure gradient on the mean and local pack Nusselt numbers is given on Fig 3 for the case of an open face.

where

$$F = \frac{c_p \rho_m \beta_y D^2}{\mu_m k H} \left(\frac{\partial p}{\partial y} \right)_{co}$$

$\left(\frac{\partial p}{\partial y} \right)_{co}$ = the constant pressure gradient over the hot face
 y = vertical distance measured from the high pressure region

Otherwise the same notation has been used as defined on Fig 1.

The theory has now been extended so that the influence of the hot face sealing characteristics can be investigated. A considerable programme of basic experimental work is in progress at TNPG to provide substantiation of this theory. Initial results obtained on reactor prototype insulation for the floor of Hinkley 'B' tested in high pressure carbon dioxide are encouraging.

Mechanical Design Aspects

In both the foil and wire mesh and the fibre insulants developed for the Oldbury and Hinkley 'B' reactors the insulation pack was held in position and protected by cover plates. Differential expansion problems are severe and in order to minimise sliding at supports single stud supports are used in both designs. Adequate space between the cover plate when cold ensured that there was no interaction between hot cover plates. However, sliding problems cannot be eliminated as the gaps between cover plates must be bridged to contain or protect the insulant. The cover plates provide the insulant compressive loads and the frictional forces between the sliding components are reacted by the stud.

Liner Studs. — Studs are either gun welded or hand welded to the liner. The number, size and materials are subject to the following constraints:

- (a) Thermal
- (i) Stud density as expressed by the total stud cross-sectional area per unit area of liner is restricted by heat loading. In the HTR

this restriction is less severe because of the relatively high conductivity of the basic insulant.

- (ii) Single stud size influences the maximum liner temperature and temperature gradients. The insulant thickness is greater in the HTR and larger stud diameters are therefore acceptable.
- (iii) Welding problems are eased if a mild steel stud is used. However, thermal conduction and the hot face temperature conditions require the use of stainless materials. Bi-metallic studs have therefore been used extensively.
- (b) Mechanical
 - (iv) Pack compression forces result in tensile stud loads. Also due to the non-uniformity of the pack the effective centre of the pack loading is unlikely to coincide with the stud centreline and bending loads are therefore introduced.
 - (v) Bending loads on the studs result from the friction forces between the sliding surfaces and also from the weight of the cover plates on vertical surfaces.
 - (vi) Dynamic loads arise because the sound pressure level within the reactor can be high enough to introduce vibration in the insulation components.

Cover Plates. — The dimensions of the cover plates are influenced by deflection, stud loading and differential expansion problems. Edge deflection must be restricted as the sealing effectiveness of the material bridging the cover plate gaps is affected.

The acoustic response of the cover plates can introduce random cyclic plate stresses which will exist throughout reactor life and cover plate size, thickness and material must cater for the problem of fatigue and creep.

Fibre. — It is very important that the fibre used to restrict the gas movement should have adequate long term compression characteristics over the full range of reactor temperatures. Compatibility and dust content are also important considerations in the choice of fibre.

Insulation for HTRs

Theoretical and experimental work have enabled correlations to be produced for both the natural and forced convection situations. Because of the relatively low pack Rayleigh number, the thermal problems of the insulants in a helium environment are eased and height effects and hot face sealing characteristics are not so important.

Analytical procedures of the Davidson type may not be necessary for HTR insulants. It is not difficult to design the insulation so that the effects of convection are negligible but this does not necessarily result in the most economic design. Consideration of the F parameter indicates that the lower density and lower pressure gradients in the HTR (due to the reduced gas circulation) are likely to reduce the effect of surface pressure gradients relative to the AGR.

However, if after an overall cost assessment of the cooling system and insulation designs the same conductance as used in the AGR is required then, because of the high thermal conductivity of the gas, packs of two to three times the thickness of those used in the AGR are likely.

For higher temperature applications, fibres with a higher resilience than Triton are required and a dust free fibre would be an advantage. However, permeability requirements can be relaxed as a value of $R_a^{D/H}$ (Fig 2) less than 2 can be easily achieved and a wide range of fibres and fibre sizes can be considered for use in the HTR. A considerable programme of physical property measurements on fibrous materials has therefore been initiated by TNPG.

Many of the mechanical problems investigated during the development of the AGR insulation have direct relevance to the HTR insulation. Acoustic effects and stud heat loading are likely to be less severe but stiction and sliding problems demand sophisticated studies. Ideally thermal cycling and similar proving tests should be carried out in a helium environment of the reactor purity.

Elsewhere special solutions are proposed (Fig 4), as some of the materials used to date would not be suitable. These problems are discussed in detail later under Basic HTR Design.

PENETRATIONS

The integral boiler concrete pressure vessel must be penetrated at various points to allow the loading and discharge of fuel, the insertion of control rods, instrumentation etc. For the HTR a podded boiler arrangement is proposed and this greatly simplifies the water and steam penetration problems.

The major insulation penetration problem involves the standpipes (Fig 4) through which fuel and control rods are passed. These penetrations are very numerous and from the liner cooling and thermal insulation are a significant part of the total problem.

Under normal operation the standpipes contain a plug and the liner cooling and insulation problem is then small. However, the plug is removed prior to refuelling on load and each standpipe can then be considered to be a high aspect ratio insulated vertical pipe closed at the top and open at the bottom to an infinite high pressure, high temperature environment. It is the empty state which dictates the design of the insulation and cooling system.

TNPG have carried out very extensive investigations of this problem and the influence of gas, gas pressure, gas temperature and external thermal resistance has been measured for a range of pipe sizes including prototype Hinkley 'B' standpipes. In the latter case the influence of standpipe internal details on heat loading and temperature distribution has been covered. Tests on the Hinkley 'B' standpipes using helium are now in progress and tests on the much larger diameter standpipes proposed for the HTR are being planned.

Only results for the empty standpipe case with a high thermal resistance to the heat sink will be given in this paper.

Standpipe Tests

On Fig 5 test results are given for two standpipes, one of which was tested in helium and carbon dioxide, the other in carbon dioxide only. The maximum pressures and temperatures were 400 psig and 500°C.

The heat transfer coefficient in the Nusselt number is obtained

from local gas and standpipe temperatures and heat fluxes and the mean value over the height of the standpipe is used. The variation of local Nusselt number with height was not large.

$$\begin{aligned} \frac{h_g D}{k} &= \text{Nusselt number} \\ R_a &= \text{Rayleigh number} = \frac{C_p \rho_m^2 D^3 g \Delta T}{\mu k T} \\ h_g &= \text{heat transfer coefficient between gas and standpipe} \\ D &= \text{standpipe bore} \\ \Delta T &= \text{temperature drop between gas and standpipe} \\ T &= \text{mean absolute gas temperature} \end{aligned}$$

Otherwise the same notation as for Fig 1 was used.

Basic studies have also been conducted in which flow visualisation techniques were used to indicate the natural convection flows for this situation. The complexity and instability of the flow indicated by this work and also by gas temperature measurements in the high pressure gas tests suggest that a complete understanding of the problem is unlikely at the present time.

HTR Standpipes

Designs of HTR standpipes differ in two important and opposite respects from the AGR designs. Firstly, in order to allow the core fuel bricks to be removed a much larger diameter of standpipe is proposed. Secondly, the use of a downward flow core exposes the standpipe to the low temperature gas. Under present design conditions the nett effect of these differences is to reduce the Rayleigh number in the HTR by at least one order of magnitude relative to the AGR.

The thermal loading problem of the HTR standpipe is therefore of the same order as the AGR and it is partly for this reason that full scale tests are planned. An even more important reason, however, is that it is essential to know the temperatures of all the charge chute components and this can best be done by testing prototypes. The reduction of component temperatures due to the downward flow core is very significant.

Circulators

In the early magnox reactors, which were housed in steel pressure

vessels with external ducts to the boilers, the gas circulators were housed within the ducts. They were driven by shafts penetrating the pressure circuit through a gas seal. Many forms of drive were used, from steam turbine to electric motor with fluid coupling. At Oldbury, the first of the integrated designs with the whole of the pressure circuit within a single vessel, the concept was retained, with the circulator being driven by an extension shaft located in a tunnel through the vessel wall. For Hinkley 'B' however, a new concept of encapsulated motor drive was introduced, based on the successful fully-enclosed design pioneered by Howdens at Winscale AGR, with the motor located within the vessel wall and operating under full reactor gas pressure, so eliminating the need for a shaft seal.

The design incorporates many of the desirable features which have been proved on earlier designs. Firstly, the drive is a constant speed electric motor, which has considerable advantages in flexible commissioning and in providing emergency supplies. Variation of reactor mass flow at Hinkley 'B' is achieved using variable inlet guide vanes, an arrangement which is well proven in conventional circulator design, but which of course remains to be tested under reactor conditions. Secondly, the motor and the impellor form a single compact unit which can be removed relatively quickly from service, and be replaced equally quickly after maintenance and repair.

Although the rotating shaft gas seal has been eliminated, the whole of the shaft bearing assembly is in the gas circuit, and extensive development work has been necessary to design sealing arrangements for the lubrication oil system. In parallel, much of the ancillary equipment in the motor compartment, such as the water coolers for controlling the local gas temperatures, have had to be designed and manufactured to a very high standard to ensure minimum contamination of the reactor gas.

The first Hinkley 'B' circulator is now ready for prototype testing under simulated reactor conditions before final installation. The form of design has been adopted for the other AGR stations now on order, and represents a solid basis for future HTR designs.

Boilers

The boilers in gas-cooled reactor stations are much more like gas to water cross-flow heat exchangers in conventional coal or oil-fired boilers. Throughout, there has been a compromise between the desire for large face area, and so small gas-side pressure drops and dynamic forces, and the requirements for fitting them into expensive pressure shells. Manufacturing costs have influenced the compromise to a considerable extent.

Early designs were assemblies of straight tubes, tightly packed in staggered or in-line banks, connected at each end by forged bends. Welding costs were inevitably high, so methods have been developed for manipulation of continuous lengths of tube to achieve the same low horizontal and vertical pitching which is necessary in producing compact boilers.

In principle the inter-connections between tubes are relatively straightforward because of the regular pitching and both welded and clamping designs have been used. Clamps have the particular advantage of providing damping of any tube vibration and worked well with low alloy steel boilers; as higher alloy material has been introduced as gas temperatures have risen, galling problems have been encountered and TNPG now use welded connections. The lack of damping is allowed for in the extensive vibration survey now carried out during the design stage.

Relatively little trouble has been experienced by TNPG with nuclear boilers, but occasional tube failures have, of course, been experienced. In general, they have become apparent early in the life of the station, and can often be traced to individual defective welds. The tubes are blanked-off external to the reactor vessel and, while this means a small loss of heat transfer surface, no measurable operational penalty has been experienced. The siting of the boilers within the reactor pressure vessel, as at Oldbury or Hinkley 'B', inevitably raises the question of what can be done in the event of massive tube failure because of a systematic design fault. The probability of this occurring is extremely small as gas temperatures are rigorously controlled at a low level (relative to those in fossil-fired boilers) and it is now general practice to install full-flow treatment plant for the

boiler feedwater. Experience with once-through boilers is limited, however, so the boilers are protected from direct radiation from the core by a shield-wall. The calculated activation is such that access for inspection will be available throughout the life of the plant. If massive failure is detected then the boilers could be replaced under an extreme emergency procedure which would involve a major breach of the vessel. This would take a considerable time, but not longer than that required to design and manufacture new boilers.

The extent of the operation is such that it would only be tackled under extreme conditions. The main emphasis in the design is placed not on removeability but on investigating and testing existing forms of boiler to reduce the risk of a major systematic failure to an absolute minimum.

FUEL HANDLING

One of the earliest decisions in the evolution of gas-cooled reactors was to design for on-load fuel handling. The object was to achieve a plant which could be operated at the highest possible availability with planned outage for the reactor being within that necessary for the turbine. With magnox reactors the decision was reinforced by the wish to use the beneficial effects on reactivity of the build-up of plutonium and the relatively low fuel life.

The restriction of natural uranium as a fuel places a high premium on core reactivity. With a continuous re-fuelling cycle the build-up of plutonium in fuel of low irradiation can be used to offset the low reactivity of highly irradiated fuel.

There are over 3000 fuel channels per reactor in the first two stations at Berkeley and Bradwell and with an average fuel dwell time of four years, corresponding to an irradiation life of 3000 MWD/te, the annual rate of channel replacement approached 800. The effect on reactor availability of off-load fuel handling was, therefore, considerable and extremely expensive because of the very high cost of fuel in the alternative fossil-fuelled stations.

With the introduction of enriched fuel into the AGR design the

benefits of on-load refuelling on fuel life were reduced, but equivalent advantages soon became apparent. Because the reactivity of the reactor is an average of the reactivities of the individual channels, the irradiations of which are uniformly distributed over the range new to fully irradiated fuel, the overall reactivity is almost independent with time. There is no need therefore to include an excess number of control rods to control the inevitable high reactivity of the new fuel in a reactor which is re-fuelled on a yearly off-load basis. Of particular importance is the fact that all the replacement fuel is of an enrichment which is known well in advance of it being required on site. If a station, for any reason, runs only at a poor load factor, then the refuelling is adjusted by reducing the rate at which channels are changed. The level of enrichment is also reduced compared to a fuel cycle with yearly refuelling which reduces the fuel costs by \$4/kW to \$5/kW.

The decision to adopt on-load refuelling was taken in full realisation of the mechanical problem involved and the successful achievement has not been without substantial difficulty. The basic problem on a magnox reactor is to penetrate the reactor pressure vessel, locate a channel some 50 feet away, and grab and remove a fully irradiated, delicately designed, fuel element. The handling mechanisms must operate in high pressure carbon dioxide at a temperature of 400°C, replace the new fuel precisely in the right position without damaging the fuel beneath with any impact loads, then retract from the vessel leaving the pressure vessel closure secure.

During the early fuel-handling trials difficulties were experienced with the operation of some of the mechanisms because of distortions due to the uneven rates of heating, while the fuel element grabs had a very limited life. None of the reactor stations designed by TNPQ has ever suffered a loss of availability because of deviation from the planned fuel cycle and the final success of the operations can be seen from the history of the Berkeley and Bradwell stations during 1967 (Tables 2 and 3).

Table 2 Fuelling Performance at Berkeley during 1967

	Reactor 1	Reactor 2
Total number of channels refuelled in 1967	820	799
Total number of channels refuelled to 31/12/67	3518	3517
Deviation from programme on 31/12/67(channels)	- 3	+ 4
Maximum channel average fuel irradiation $\frac{MWD}{Te}$	3555	3530

Table 3 Fuelling Performance at Bradwell during 1967

	Reactor 1	Reactor 2
Total number of channels refuelled in 1967	1271	573
Total number of channels refuelled to 31/12/67	2384	2502
Deviation from programme on 31/12/67(channels)	0	0
Maximum channel average fuel irradiation $\frac{MWD}{Te}$	3696	3450

The performance during 1967 has been chosen to illustrate the re-fuelling rates which can be achieved after the initial difficulties have been overcome. The very high rate on Reactor 1 at Bradwell is indeed due to the final clearing up of a residual back-log from earlier years. During these early years there were considerable difficulties at both stations, but without detriment to the overall load factors (based on guaranteed output) which are given in Table 4.

Table 4 Annual Percentage Load Factors

Year	1963	1964	1965	1966	1967	1968	1969
Berkeley	45.7	53.1	89.3	76.9	86.8	91.9	89.4
Bradwell	66.7	79.3	86.9	82.3	90.3	81.3	82.3

The advantages of on-load refuelling will be as important for the HTR as for the earlier gas-cooled reactors. The reactor is being specifically designed to achieve these advantages, for example by adopting

downward gas flow such that the refuelling equipment will operate at the relatively low temperature of 300°C . There are major differences from earlier designs in that the mechanisms must work in helium, and the fuel element is heavier and must be loaded into free-standing columns. With the experience already gained, however, there is every chance that the early operational difficulties will be significantly less than those experienced at Berkeley and Bradwell.

THE BASIC HTR DESIGN

A sketch of the reactor section is given on Fig 4 and illustrates the main features of the design. The object of the design is to exploit the natural advantages of the HTR - high ratings and gas temperatures - while taking full account of existing experience on gas-cooled reactors in order to reduce risks and produce a reliable station.

The basis of the pressure circuit design is the pre-stressed concrete pressure vessel, but in a new departure for TNPG the boilers are located in vertical pods in the vessel wall. The main reason for this is economy, for the high gas temperatures have so reduced the boiler heat transfer surface that we no longer have to arrange the units in an annulus round the core in order to get sufficient frontal area. The boilers are very compact and fit into four of the eight available pods, the other four being used for helium storage.

Many detail designs of boiler are being investigated but the preferred arrangement at the moment is very similar to that used for the AGR. Alternative boiler designs are being investigated based on the principle of helically coiled elements, which give a more compact overall unit. They represent a marked change in manufacturing procedure, however, and until much more experimental work has been done there are several technical unknowns. These are concerned with the variable pitch between the tubes and thus the uncertainty of predicting heat transfer and vibration characteristics, as well as the erection of the preferred welded tube-to-tube connections.

The coolant gas flow is downwards through the reactor to avoid having hot gas (750°C) anywhere near the standpipes in the top of the

vessel, and to enable on-load refuelling in an ambient temperature of 300°C. The standpipes are difficult to insulate in themselves, and they contain the vital control rod mechanisms which must at all times be prevented from over-heating.

The overall insulation within the vessel has been divided into two quite distinct divisions. Cooling flows at 300°C have been designed so that all the insulation protecting the vessel liner sees gas of a maximum temperature of 400°C. This insulation will be designed in a manner similar to that used at Hinkley 'B' AGR, taking due allowance for the helium atmosphere. As the reactor control system tends to keep the reactor inlet gas temperature constant during all normal running sequences, this insulation will experience a minimum of thermal cycling and can therefore be guaranteed for long life.

The function of the high temperature insulation, which covers a restricted region from the core outlet to the boiler inlet, will be limited as far as possible to restricting the passage of heat from the hot to the cold gas streams rather than to maintain component temperatures. Any breakdown will result only in a loss of temperature potential, and therefore station efficiency, and represent no hazard to the safety of the plant. It is hoped to use solid insulants, because of doubts about the resilience of fibre materials, at high temperatures. The reactor core, for example, will stand on a firebrick hearth.

The circulators are located in penetrations through the top slab of the vessel above the boiler pads. As these penetrations are sufficiently large to allow the passage of a boiler, during erection, and as a remote possibility during removal for replacement, there is sufficient space to accommodate all the circulator auxiliary services. The design of the circulator will most probably follow that being developed for the AGR, but all of the assumptions implicit in that design are being re-examined. For example, it may prove preferable to go back to a variable speed electric drive if friction and wear experiments in helium show any potential difficulties with an inlet guide-vane mechanism.

The HTR is significantly different from earlier gas-cooled reactors

in the use of particle fuel and the helium coolant. Outside the immediate environment of the core, however, much of the engineering is little different in character to that of previous gas-cooled reactors. The experience gained on these reactors is being used to evolve an HTR design with the same reliable high load factor operation that has already been achieved at Berkeley and Bradwell. By the end of 1970 TNPG will have completed such a design for a 600 MW(E) power station to be in operation in 1975.

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1. Foil and Wire Insulation - Variation of $\bar{N}_u$ with R_a - Natural Convection.

2. Isotropic Insulation - Variation of $\bar{N}_u$ with $R_a D/H$ - Natural Convection.

3. Isotropic Insulation - Local and Mean Nusselt Number - Forced Convection.

1 CORE	5 CHARGE MACHINE
2 CORE SUPPORT	6 CHARGE MACHINE CHUTE
3 CIRCULATOR	7 CONCRETE PRESSURE VESSEL
4 BOILER	8 STANDPIPE

4. Reactor Elevation - HTR.

5. Standpipes - Variation of $\bar{N}_u$ with R_a .

DISCUSSION

G. Meijer: Is your evaporation in the steam generator downhill or uphill?

Do you plan to do any test for downhill evaporation?

B. N. Furber: The flow in the boiling section in the TNPG design is uphill.

Tests are in progress to investigate the heat transfer and stability aspects. It may also be of interest to report that Professor W. B. Hall (Manchester University) is investigating supercritical boiling. Results to date suggest that downhill supercritical boiling is more consistent than uphill supercritical boiling.

M. Dalle Donne: You mentioned that the Nusselt number at the insulation and at the standpipe is about 10 times lower with helium than with CO₂. In reality the thermal conductivity of helium is 7 times higher than that of CO₂, so that the heat transfer coefficient is about the same and the heat losses through the insulation are about the same.

B. N. Furber: The reduction of Raleigh number by a factor of 10 applies approximately to the TNPG standpipe design. In this design a large standpipe diameter is required to enable fuel bricks to be changed. Assuming this factor of 10, then the Nusselt number is reduced by approximately 2.5. This does not compensate for the increased thermal conductivity of helium. Nevertheless, the main point is that the increased heat transfer coefficient in the TNPG standpipe design can be accommodated.

R. Huddle: What materials do you intend to use for the hot facings of ducts and liners? Do you think there is a material problem?

B. N. Furber: In the low temperature regions the hot face materials will be similar to those used in the HTR. In the high temperature regions ceramic materials (for example, silicon nitride) are being seriously considered.

M. Bender: The HTGR reactors of the Dragon type operate at temperatures that may be several hundred degrees above those of the AGR type.

Could you indicate what effect these elevated temperatures would have on the insulation requirements and characteristics. (Supplementary note: Many people do not realize that concrete vessels are usually designed so that coolant gas adjacent to the concrete is at reactor inlet temperatures and the insulation performance is therefore not very sensitive to core outlet temperature conditions. Nevertheless, there is some increase in coolant temperatures over those in the AGR systems, and my impression is that these do tax the capability of reflective insulations because the seals between insulation panels are difficult to hold to low leakage levels. This is the main reason why fiber insulations have become attractive. The speaker evidently did not understand the question and it did not seem worthwhile to belabor the point. M.B.)

B. N. Furber: There is no basic thermal problem resulting from the higher gas temperatures of the increase of thermal conductivity if temperature is not linear. The design conductance results from a study and it is probable that insulation thickness could be 4 to 6 in. or 2 to 3 times A.G.R.

HTGR NEW AND ADVANCED DESIGNS

(Session III)

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THE HIGH TEMPERATURE REACTOR DEVELOPMENT IN GERMANY
(PRESENT SITUATION, PROGRAM AND FUTURE ROLE)

Dr. H. Krämer

1533000

ABSTRACT

Although HTR-development in the German Federal Republic is being supported by the government to about the same extent as fast-breeder development, and about one thousand million DM will be spent by 1975 according to the present plans, the utilities continue to show relatively little interest in the HTR system.

New developments, however, especially in the field of reactor safety and pollution indicate a potentially broad future market for the HTR alongside LWR's, as well as for other reactor types not directly involved in this development program. These market chances are coupled with the specific characteristics of the HTR-system, namely; favourable economy, high inherent safety, a very much better fuel utilization, and less coolant water demand than for the LWR's. Some of these characteristics will become greatly important in the Federal Republic of Germany because of the restricted number of sites with sufficient fresh water cooling and because the high population density means that sites very near to cities must be taken into consideration more and more.

With the realization of the first large HTR-plant in the Federal Republic of Germany, the HTR development will enter the decisive phase in this country. The final decision for this plant, the 300 MWe THTR, is expected by the middle of May 1970. Of great importance also is the forthcoming association of the firms GHH, KRUPP, and BBC.

The work on the HTR is proceeding in three steps. During the first step, work was concentrated on two cycles plants. This initial development work, now essentially completed, was directed towards the construction of the AVR-reactor and the design of the THTR. During the construction phase for the THTR, further component and fuel element tests will be made within the framework of a specific industrial development program.

In the second step, the coupling of the HTR in closed cycle directly to a helium turbine will be investigated. Such a development program has already started and will be finished in 1974 with the preparation of the construction documents for a 600 MWe plant. Some very important projects within this program are:

1. Construction and operation of the 22 MWe KWSH-plant. The commissioning of this power plant is scheduled for 1974.
2. Construction and operation of a fossil fired 50 MWe power plant employing a helium turbine.
3. Establishing a multipurpose test facility which particularly includes a test section with a helium turbine representative of a 300 MWe plant, with which full scale components of the primary circuit can be tested under realistic operating conditions.

This development program is expected to reach the stage of commercial application by 1980.

During the third development step, the direct use of thermal energy from HTR's for chemical processes will be investigated. Such processes are the production of hydrogen for use in steel manufacture by cracking methane, heavy oil or coal. Another application is the production of raw materials for the chemical industry. This work is presently in the project definition phase. We assume that in 1971 the development work can be started on full scale.

The above mentioned activities will be supported by a broad materials development program.

The paper gives a short report on the present status of HTR development in Germany. The main problems which are still open are pointed out and the planned activities are mentioned.

INTRODUCTION

The primary and still most important goal of the present HTR development is to provide the utilities with an economic and safe nuclear heat source, which allows to realize a modern steam turbine process. We know that the saturated steam turbine does not represent a final solution, inspite of the fact that they have been improved very much in the frame of the LWR development.

On the other hand the investigations of the last few years have shown that HTR has further development potential beyond the realization of a modern steam cycle. This potential consists first in the use of the more progressive and in principle, more simple gas turbine process for electricity production. Another potential development is the direct conversion of nuclear heat into chemical energy in form of a steam reforming process.

This means, that today we have to consider the HTR as a reactor family more as a single plant typ. The different variations offer due to their specific properties broad possibilities of application not only on the electrical energy market but also on the primary energy market. This potential is of great importance because of the fact that the primary energy market today is nearly 4 times larger than the market for electrical energy to which nuclear energy has thus far been limited exclusively, and that in 30 years as Fig. 1 shows, it will still be as large as the market for electrical energy.

CHARACTERISTIC ITEMS OF THE GERMAN HTR-PROGRAM

Without any change in the most urgent task, namely to push the first HTR-concept (with steam turbine) from the experimental into the commercial phase.

In West Germany development work for the more advanced concept with direct cycle and for the industrial plant has already been started of course with different priorities and different rates.

The initiative for this results from the following reasons: In West Germany the light water reactor has meanwhile established very strong industrial support and has reached an astonishing position in the energy market. It has been estimated, for example, that for this year approximately half of the new power plant installations will be shared by PWR and BWRs. Thus new economic criteria have been established against which each further nuclear development in West Germany must be matched. A general success for reactor companies supporting the HTR for that reason will only be achieved if extreme efforts are undertaken and if they are focussed as soon as possible to HTR concepts with the highest economic potential.

I may recall that the HTR concept has had in West Germany rather modest support by the utilities untill recently. This situation is changing slowly now. One of the reasons are the growing safety requirements for nuclear plants in high density populated areas such as West Germany.

A further important reason is given by the situation for cooling water provision in West Germany. Several analyses indicate a termination of fresh water cooling in foreseeable time. In LWRs the use of cooling towers leads to essentially higher cost penalties than in other concepts and would clearly affect the position of this plant type in industrial competition.

As far as we know today HTRs with helium turbine should have the largest potential of all nuclear concepts with res-

Fig. 1 Primary Energy Demand in West Germany until 2000

Fig. 2 Schemes for Industrial HTR-Plants

pect to requirements for cooling water. Even air cooling could come into consideration. A quickly and completely exhausting the potential of the HTR concept by developing the direct cycle system already today should therefore make the marketing of the HTR essentially easier.

The interest in development work for the third generation of HTR plants, i.e. industrial complexes comes from industrial groups outside of the electricity industry whose economic and political influence can have a strong impact on the whole HTR development. The reason for this interest lies partly in the hope of maintaining the basis for their industrial existence (coal) partly in the prospect of broadening the market for their products (brown coal, natural gas), and also in the possibility of using more new economic industrial processes (steel making, production of chemical raw materials etc.).

In Fig. 2 as examples two processes are shown which could be realized with an HTR industrial complex. The lower drawing shows a scheme for steel manufacture: natural gas is cracked into hydrogen and carbon dioxide in a tube cracking turnace at 900° - 950° C using nuclear heat. The hydrogen is piped then to a plant for direct reduction of iron oxide into iron sponge that will be further converted to steel in an electric arc. The necessary electric energy for the arc is produced in the same HTR plant by a steam turbine dycle. The steam generator following the tube cracking furnace in the primary helium circuit.

Similar industrial complexes are possible for making acetylene, ethylene or other raw materials from natural gas. Instead of natural gas also oil or possible goal could be used in these processes.

The upper drawing in Fig. 2 shows a possible scheme for hydrocracking of oil to gasoline.

HTR WITH STEAM TURBINE

Fig. 3 shows a survey of the total program of West Germany subdivided along the lines of development already mentioned. The most important source of construction- and operation experience for the two-circuit line and simultaneously the starting point for the other development lines is the 15 MWe experimental power station AVR, that at the end of 1967 started power operation and since then has operated with satisfactory results as described in paper 2/103.

On the basis of the main design principles of this plant during the years 1964-68, the 300 MW THTR plant has been developed by BBK and KFA in association with EURATOM.

More than a year ago BBK made a bid to HKG, a consortia of several utilities for this THTR power plant. The contract negotiations for the construction of the plant, in the course of which not inconsiderable difficulties arose are in the very final stage now. The licensing procedure has been running since the beginning of this year and has presented no apparent difficulties thus far.

In the THTR project spherical fuel elements containing uranium/thorium oxide will be used in an on-line refuelling scheme. The elements have become so well developed in the last years that the supplying company, NUKEM, is in the position today of giving far-reaching guarantees on the achievable burn-up.

In parallel to the construction of the plant a R and D-program will be carried out, in which all the main components

Fig. 3 Main activities Within The HTR-Development Program

Table 1. Typical Datas of First-Generation HTR Plants

		THTR FR-Germany	Fort St. Vrain USA	Mark III Great Britain
Electrical output (Generator net power)	MW	307,5	330	640
Gas temperature core inlet	°C	250	404	300
core outlet	°C	750	776	800
Gas-pressure (maximum)	kg/cm ²	40	49,1	56,25
Power density in the core	MW/m ³	6	6,3	6,3
Specific Burn-up	MWd/t U	115.000	100.000	60.000
Type of Fuel		UO ₂ -ThO ₂	UC ₂ -ThC ₂	UC ₂ -ThC ₂
Diameter of fuel kernel	μ	400	400	800
Direction of gas flow in the core		downwards	downwards	downwards
Core-diameter	m	5,6	4,7	8,79
Core-height	m	6	6	6,8
Prestressed Concrete PV		cylindrical	cylindrical	cyl. with pods
inner diameter	m	15,9	9,45	9,5 (core-part)
inner height	m	15,3	22,8	16 (core-part)
Live-steam pressure	kg/cm ²	181	169	174
temperature	°C	530	538	565

will be tested in detail. Among these components are the fuel charging device, the regulating and shut-down rods, parts of the graphite structure, the hot gas canal between core and the steam boilers, the burn-up measurement plant, the boilers and blowers.

Apart from the fuel elements and the pertinent fuel charging device the THTR shows as is described in more detail in the next paper (2/113) a wide-spread agreement in the design with corresponding concepts of the GCA and those of the British companies (see table 1). The time schedule for the first commercial HTR plant in West Germany will be fixed exclusively by the responsible firms BBC, BBK and GHH. Up to now no official statements have been divulged. Quite considerable efforts for further development on an purely industrial scale has yet to be done and it is certain that an order for the first commercial plant is not to be expected before a trustworthy appraisal on the validity of the cost calculations for THTR have been made. The earliest date for this seems to be two years after the beginning of the construction phase for the THTR, but probably an even longer period will be necessary.

It is apparent that the main efforts within the steam cycle line to reach the commercial stage in West Germany are concerned with the concept of spherical fuel elements. However investigations are being made also for reactor cores with block type fuel elements. The question, which fuel concept will have the larger longterm potential in Germany is quite open, it also depends on the further results of the development for an HTR direct cycle plant (HHT) which is being performed for the time being in two parallel lines one for the ball and one for the block type fuel concept.

HTR WITH HELIUM TURBINE (HHT)

HHT development work is based on the following main projects.

- 1.) The experimental plant KWSH, a 22 MW nuclear plant with helium turbine using a non-integrated arrangement and rod-shaped fuel elements.
- 2.) A 50 MWe helium turbine plant in Oberhausen with a fossil fuel fired heater.
- 3.) A test site with 4 installations which have been planned in such a way that they supplement each other with respect to magnitude and availability. Although the experimental plant KWSH which has been ordered one year ago should furnish important data on the operational behavior of a typical direct cycle HTR plant, in particular on its regulating and fault behavior, and although it should in addition offer the possibility of testing prismatic fuel elements, KWSH has come under discussion again. This discussion is still in full swing. Final decisions will be made only in close coordination with the special objectives of the general HHT development.

In the 50 MW plant in Oberhausen that will be ordered by RWE and the city of Oberhausen within this year will overtake the function of a demonstration plant. Questions of the long-time behavior, availability and maintenance of helium turbines will be investigated there. Simultaneously this plant offers the possibility of evaluating the transient behavior of the main components in the helium turbine circuit experimentally.

The design of the different machines of the HHT test site and their specific tasks are described in paper 6/117. I should say here that this test site will be the central point for extensive component work.

The main activities are dealing with problems of the turbine, (the bearing, sealing and the rotor and blade cooling), and problems of the hot gas ducts between the reactor core outlet and the turbine entry (insolation, expansion devices, valves and fittings).

The mass flow for the different plants of the test site covers the range from 3 to 250 kg/sec. The temperatures vary in the region from 400 to 850°C probably even 1000°C. The available diameter of the test sections lies between 0.2 and 2 meters. It is being established at KFA by the industrial firms BBC and GHH. These companies will operate also the plant together with KFA. First experiments can be started within the next few months, the completion of the whole test site will not be achieved before end of 1972.

Parallel to the construction and operation of the above mentioned installations, theoretical and experimental investigations have been started in meantime. These will for the most part be concerned with two problem areas, 1. defining the lay-out of the plant and 2. the plate-out of fission products in the turbine circuit, a knowledge which is of decisive significance for the evaluation of HTR direct cycle power plants.

The possible lay-outs are still judged in a rather different way from country to country, as the meeting of the BNES in London again showed just recently, and are under much discussion. The question of the best design is not yet decided even inside individual working groups. Under discussion are non-, half-, and fully integrated concepts. Inside our working groups presently a circuitous and detailed evaluation scheme is being set up that can be applied systematically to the different concepts. Final results can not be expected until

the end of this year. Particular details on it will be presented in paper No. 5/115.

A survey of the work planned for studying the behavior of fission products in the cooling circuit is given in paper No. 7/127.

At the central point of this work is an in-pile experiment that will be started in the reactor PEGASE (Cadache) in the middle of this year and that will simulate as well as possible the operating conditions of a one-circuit power plant.

The knowledge from these investigations as well as that worked out in other afore mentioned projects will be utilized for the design work of a 600 MWe plant whose upper helium gas temperature has already been established at 850°C . The completion of these designs is expected by 1974. This means that the commissioning of the first large HHT plant in West Germany can not be expected before the end of this decade.

THE INDUSTRIAL HTR - PLANTS

The development work for an industrial plant, the third generation of HTRs, was started more than one year ago and has now reached the status of a project definition phase. We assume that the proper development program can start after a positive conclusion of this phase in the course of next year. This program will encompass the following activities:

- 1.) Experimental and theoretical thermodynamic studies in context with the design work for a tube cracking furnace, beginning at a single tube (10 m long, 0,1 m diameter) Later at a whole tube bundle.

2.) Coal gasification experiments using hydrogen at 100 bars in several installations of different size. (Laboratory scale - 1 kg coal/h, - half technical scale - 50 kg coal/h, and finally in a pilot plant of 1 to 2 t coal/h)

3. Experimental investigations to the kinetics of reactions between coal and steam (steam reforming) and about the reactivity of coals having different qualities.

4.) Further development work for reactor components with respect to reach higher gas temperatures (950°C).

In addition to KFA, the companies Rheinische Braunkohlewerke, Cologne, and Bergbauforschung Essen are involved in the development work during the first phase.

HTR MATERIAL- AND FUEL ELEMENT DEVELOPMENT

The development work mentioned so far is , as shown in Fig. 3, accompanied by a broad HTR-material and fuel element development program. This program includes basic work on the understanding of the irradiation behavior of all fuel element components - (partly in close cooperation with DRAGON), the collection of material datas that are required for the fuel elements design work, and the development of manufacturing techniques for specific fuel element components. A further important point of this program, which is supported by NUKEM and KFA, is related to the preparation, performance and evaluation of specification tests for the THTR-, KWSH- and the HHT-project. Since details of this program will be reported in paper No. 7/128, the particular points and the special problems can be dispensed with here.

REPROCESSING

In order to give a complete picture of HTR development in West Germany, one must still touch upon the important work in the area of reprocessing (given in detail by Dr. Merz in paper No. 1/136). This work includes the head-end and the recovery of fission materials, the latter being related only to the uranium-thorium-cycle. Out of a great number of different techniques that were worked through in the first phase of this project the burn-leach method as head end and the thorex process for the uranium separation have been finally selected as the most favorable ones for detailed investigations. Dry techniques are recommended to later development work. The investigations to date have still the status of laboratory experiments, however they are being performed already with hot material and within a complete scheme. The results obtained have been thus far satisfactory, so that design work for a pilot plant could be started meanwhile. This plant should reach a throughput of 2-3 kg/day in 1973 which is enough to process the burned fuel of the AVR.

There is not yet a definite conception on the construction of a big prototype plant for reprocessing thorium containing HTR fuel elements. Several variations are possible and are being analysed presently. The total development of nuclear energy in West Germany will influence the decision but also the question of how quickly the uranium-thorium cycle can substitute the low enriched uranium cycles will play a role at least in Europe.

PROSPECTS

Only a small part of the activities mentioned are already completed, namely, the AVR-project and the development of the THTR. The others are still in progress and a part of

them has just been started.

Because of the relatively far-reaching goals very much and unfortunately the most decisive work is still in front of us.

The foreseen funds which are quite considerable for Germany are, at least in principle, agreed to by the pertinent federal ministry . In fact the HTR-development in West Germany has meanwhile reached the same priority as the development of the liquid cooled fast breeder.

These facts oblige us to analyse regularly and very seriously the whole program that means to put up the question: Whether the existing financial and personel base is sufficient for a sucessful completion of the mentioned activities and whether these activities are in a reasonable relation to the aims. During the next weeks we will undertake such an analysis once more.

As the HTR line can not rely on extensive military developments, which was the case for the commercial exploitation of the LWR-systems, in Germany we are worrying about a possible overcharge of the disposable forces already in the course of introducing the first HTR generation into the free energy market as it exists in our country. The fusion of all german industrial companies supporting the HTR which is being completed within the next months, gives great hopes in this respect but it is possible that only an even greater, that means international industrial base can guarantee a good chance for the commercial exploitation of the HTR. An effective international cooperation between the competent countries should therefore be put into consideration much more in the future.

DISCUSSION

F. L. Culler: 1. Is 1600°F (approximately 850°C) high enough temperature to drive the water gas reaction? Will the helium not have to be at considerably higher temperature to obtain good yields of wet fuel gas with the steam carbon reaction? 2. Have irradiated graphite fuel element samples been burned and leached?

H. Krämer: 1. 850°C is not a sufficiently high temperature for a steam-reforming process, 900-950°C will be necessary. We are hopeful that such gas temperatures can be reached within the next 5-7 years. 2. Yes, in hot cell experiments, detailed descriptions of the experiment will be given in the papers by Merz and Hackstein.

DESIGN, FEATURES AND ENGINEERING STATUS OF THE THTR 300 MWe
PROTOTYPE POWER STATION

H. Oehme J. Schöning

1415200

ABSTRACT

Brown Boveri/Krupp Reaktorbau Ltd. is developing a line of high-temperature helium-cooled pebble-bed reactors, with completely integrated primary system. The feasibility of the concept has been demonstrated by the AVR experimental reactor, which has been supplying electricity to the grid since December 1967. The next stage in the development is the 300 MWe THTR, which has the same design characteristics as the AVR. However, various components were modified because of the increased dimensions of the THTR, such as the prestressed concrete pressure vessel. The design of the THTR, its safety concept, and supporting development tests are described, as well as the status of engineering and construction arrangements.

GENERAL CHARACTERISTICS OF THE 300 MWe THTR

The main activity of the Brown Boveri/Krupp Reaktorbau Ltd. is concentrated on a thorium high temperature reactor line based on spherical fuel elements. The main characteristics of this reactor line are the following:

- spherical fuel elements
- helium as coolant
- high primary gas temperatures up to 800°C
- primary system completely integrated

The spherical fuel element permits continuous fuel loading during operation. Each individual fuel element has a negligible amount of reactivity.

The feasibility and advantages of this reactor concept have been demonstrated by the construction and operation of the AVR experimental reactor (15 MWe) which was brought to criticality for the first time in August 1966. Since December 1967 AVR has supplied electricity to the grid, and during 1969 the plant achieved an availability of 72 %. The thorium high temperature reactor (THTR) possesses essentially the same construction characteristics as the AVR (Slide 1). However, based on the experience gained with AVR, and in view of the higher power of THTR the following components were introduced or changed:

- the prestressed concrete pressure vessel
- the core structure
- the shut-down system
- the fuel element circulating system

The 300 MWe THTR prototype reactor is designed and optimized as a direct predecessor of a reactor of commercial size (Slide 2).

THE INTEGRATED PRIMARY SYSTEM

The reactor core and all main components of the primary system - blowers, steam generators, shut-down system -, excluding the fuel circulating system and gas purification plant, are completely integrated in a prestressed concrete pressure vessel. Access to these components is not foreseen although they can be removed in case of failure or of inspection or maintenance work. All other components and facilities are located outside the prestressed concrete pressure vessel and are accessible during reactor operation (Slide 3).

The reactor core consists of approximately 700 000 spherical fuel elements of 6 cm diameter. The cylindrical core of 5,6 m diameter and 6 m height is formed by a graphite wall which acts as reflector and surrounds the core on all sides. The bottom reflector is conically shaped with an inclination of 30° and opens into a central discharge tube of 800 mm dia.

Graphite is a well-known material proven in many reactors. The THTR reflector is exposed to a higher radiation dose (maximum total dose $2 \cdot 10^{22}$ nvt) and to a considerably higher temperature. Apart from that and in addition to its usual function as a reflector, and as nuclear and thermal shielding, it must perform the mechanical function of a supporting and containing structure for the pebble bed. By an appropriate choice of the graphite (considering the important influence of the cementing resin) it is possible to remain within controllable dimension changes of the structure since the determining fast neutron flux decreases very rapidly in the reflector so that a noticeable shrinkage can occur only up to a depth of 10 - 20 cm of the inner graphite layer. With the selected graphite type and the total maximum dose to be expected a maximum shrinkage of 2 % and an corresponding growth to the initial dimensions was taken as a design basis. Because of all these requirements, special measures for the core structure are necessary, to provide the necessary flexibility of the graphite structure for temperature- and radiation-induced dimensional changes, thus avoiding possible compressive forces while insuring the stability of the total structure. These requirements were met by relatively small blocks specially shaped and arranged in columns and by connecting and fixing the blocks with wedges and dowels (Slide 4 and 5).

The prestressed concrete pressure vessel acting at the same time as pressure vessel and as gas- and pressure tight safety containment, includes all main components of the primary circuit. It is a cylindrical vessel of 16 m diameter and 15 m height with a wall thickness of approximately 5 m. Prestressing is achieved with annular and vertical stressing cables having a guaranteed rupture load of 890 Mp. The prestressing cables are inserted in sheaths embedded in the concrete and are grouted after prestressing to insure sufficient protection against corrosion.

For the pressure vessel a relatively conservative design was selected, which has been proven in numerous large-scale constructions and has been and will be tested in a number of model tests (1:5, 1:45, 1:20). The test result will be incorporated in the large-scale construction. For further projects, while retaining the same vessel shape, various technical and cost-saving improvements are foreseen, such as transition to a horizontal wire winding prestressing system and reduction of the inner dimensions by an optimum arrangement of components. It is possible that further economic advantages may result from the podded design; the pressure vessel cost and the technical safety requirements are approximately equal for both vessel types.

For the introduction of blowers, steam generators, absorber rods, pipes, etc. the prestressed concrete pressure vessel is equipped with penetrations. These penetrations are reenforced by shutter-tubes designed to absorb the forces in the area near the penetrations. The largest openings of 2,25 m diameter for the steam generators are located in the top slab of the vessel.

Gas-tightness is achieved by application of a metal liner of 20 - 30 mm thickness serving at the same time as the inner shuttering for the prestressed concrete pressure vessel during construction. For the liner material special properties are required, such as high elasticity, good weldability, and high yield strength.

An insulation on the inner wall and a water cooling system welded on the outside of the liner protect the steel liner and the concrete against excessive temperature effects. As insulation a metal foil insulation (stainless steel) proved to be the optimum solution since with this insulation purity requirements can be easily met, irradiation behaviour of the materials is sufficiently known, mechanical properties are satisfactory, and aging and corrosion are not to be expected. The thickness of the heat insulation of the liner is approximately 70 mm.

Six parallel single-stage radial blowers each supplying one steam generator, circulate the coolant gas. The blower is designed as an insertable unit. The impeller is mounted in an overhung position and is oil-lubricated. A baffler-gas system avoids the penetration of oil into the primary gas. Each blower is equipped with a combined bypass and shut-off valve system to allow connection of a single blower while other blowers are operating, trimming of coolant gas flow through one steam generator under constant blower speed, and shut-off of one line in the event of failure of one steam generator. As drive a bipolar 3-phase squirrel cage motor was selected which is characterized by simple design and high operational safety. Blower power is controlled by variable frequency. Three blowers are supplied by one turbine generator set. In the event of failure of the two turbine sets, operation can be maintained with constant speed by the mains power supply. In addition, transformer sets are provided, permitting start up and emergency operation of individual blowers (Table 1 and Slide 6).

Table 1. Blower Data

Number of units	6
Delivery per circulator	49,25 kp/sec
Delivery in volume	14 m ³ /sec
Pressure at suction duct	38,9 ata
Pressure increase	1,1 ata
Temperature at suction duct	250°C
Circulator power	1780 kW

Six steam generators connected in parallel transfer the heat generated in the core to the secondary water-steam-circuit. The main steam generator data are listed in the table (Table 2).

Table 2. Steam Generator Data

Number of units	6
Diameter of tube bundle	2 000 + 55 mm
Height of bundle	max. 12 500 mm
Gasflow through the generator	upwards
Total length	17 800 mm
Output per unit	128 MW
Hot gas temperature	750°C
Cold gas temperature	250°C
Gas pressure	39,35 ata
Pressure drop at gas side	max. 0,45 ata
Gas mass flow	49,25 Kr/sec
Live steam conditions	335°C / 190 ata
Feed water conditions	180°C / max. 240 ata
Reheating	
at entry	365°C / 56 ata
at exit	535°C / 50 ata
Steamflow	153 t/h

The helium flows through the steam generators in an upward direction. The tube bundles of superheater, reheater, evaporator, economizer are arranged in gas flow direction. The steam generator operates in a once-through-system. The water steam-flow through economizer, evaporator, and reheater is in counter-flow direction, the superheater in uniflow direction. Three steam generators are connected in one main group, which is equipped with a common feed-water line, a start up flush-tank, and a common cold reheater line. To insure safe emergency cooling and removal of decay heat from the core each steam generator can be operated independently of the other steam generators, i.e. each is equipped with separate shut off and control valves. Each tube bundle of the steam generator is subdivided into forty independent systems which are individually led through the top slab of the prestressed concrete pressure vessel

and connected outside of the vessel. Hence, in the event of a tube failure each system can be isolated from outside. The steam generator is so designed that it can be replaced.

The control and shut-down system is designed so that the reactor load can be varied between 40 and 100 % and the shut-down reactivity required to keep the reactor in the cold critical state is available. Fine control is carried out by 36 rods which are freely moved in bore holes in the reflector. Shut-down of the reactor under both normal and fault conditions is effected by 42 core rods which are directly inserted into the pebble bed. This procedure was verified by extensive tests on a complete model 1:6, 1:10 and on sectional models in 1:1, 1:2, 1:3. This proved that the rods can be directly inserted into the pebble bed under all operating conditions of the reactor and neither the fuel elements nor the absorber rods themselves are exposed to excessive stresses. Each core rod is driven by a pneumatic step drive, achieving safe insertion at a step size of 2 cm and a step frequency of 0,3 sec. For rapid shut-down a long-stroke pneumatic system is provided, with an insertion speed of 30 cm/sec. Each drive has a separate supply system with redundancy (Slide 7).

The THTR fuel element circulating system is of special interest. It is not integrated in the primary circuit and in contrast to reactors with rod type fuel elements, requires no machinery inside the core for charging, exchanging, and discharging fuel elements. For the downward motion of the fuel elements from the core (fuel element discharge) gravity is used and upward movement (fuel element charge) is achieved with pneumatic energy by helium from the primary circuit. For the construction of the fuel element circulating system a building block system, composed of conventional units was used (Slide 8 and 9).

HELIUM TECHNOLOGY

Because of their special requirements, the components involved in the helium system required the development of a new technology which can be roughly divided into three problem areas:

- helium purification and purity
- helium leak-tightness
- lubrication and friction in an extremely pure and dry helium atmosphere

According to the present state of technology, these problems can be considered essentially solved. For selection and application of components, however, a number of aspects must be taken into account, which have proven to be advisable, on the basis of knowledge and experience gained up to now, especially through construction and operation of the AVR. The following aspects are of importance:

- good accessibility to the components needing maintenance (location outside of radiation area)
- good maintenance facilities for systems with flanged and screwed joints (plastic material)
- protection of the sensitive instruments against dust and dirt caused by assembly, through use of suitable filters
- protection of the helium against pollution from the operating components (oil, air, etc.)
- simple and conservative design of systems
- well-defined and separated tasks for individual systems, thus allowing uncomplicated design
- use of conventional proven components

These fundamental aspects partly result from special newly developed designs which were carried out to achieve an optimum solution of the reactor concept of the gas cooled high temperature line.

The mechanical impurities which are mainly produced by the gas circulation plant the fuel element abrasion in the core and the assembly protection and which impair the leak tightness and the control exactness of a number of valves, can be controlled by mechanical filters but even more effectively by restricting or removing the impurity sources. The impurities from the gas circulation facilities can be largely avoided by barrier gas systems or oil free blowers and compressors (gas-bearings blowers, membrane compressors and dry operation compressors). The impurities produced by assembly work are reduced to a minimum by careful assembly (freedom of grease, scale, and corrosion of all helium-carrying systems) in appropriate clean conditions standards. For this purpose pre-assembly in the workshops of the suppliers must be demanded. The dust which is unavoidable due to fuel element abrasion is eliminated by appropriate mechanical filters in the gas purification plant.

The wear problems occurring under these specific reactor conditions are solved by special constructions (cageless ball bearings, special bearing clearances, dry lubricated structures) with appropriate auxiliary measures such as barrier gas systems.

For safety and economy reasons, the problem of leak tightness is of special importance for the HTR. Although welding is the most simple, most economic and safest solution, it is only applicable for static seals and this applies for operational reasons only in some cases. The solution adopted for dynamic seals is to be demonstrated for two especially critical cases, that is gas tightness by valves and gas tightness in the event of rotary motion (momentum of rotation). In both cases a satisfactory solution could be achieved only by redesigning the components in parallel with sealing material and lubricant developments for these components. Because of the favourable development results of plastics materials such as Viton, Kel-F, Vespel, RCH 1000 all now available, which in addition to sufficient radiation and temperature resistance, have satisfactory mechanical properties; the same applies for lubricants.

The first example concerns a simple, safe, and economic gas shut-off bellows valve (leak tightness in the valve cap is achieved by a specially mounted plastic material). (Slide 10)

The second example concerns a gas-tight rotary drive. Also in this case, special importance was attached to safety and economy. In this type of solution, the gas tightness is achieved by using grease which forms a considerably less complicated fluid seal. Higher pressure relative to system pressure, grease expansion due to temperature changes, and re-lubrication are effected by pressure reservoirs (Slide 11).

SAFETY CONCEPT

In this connection only those facts will be mentioned, in which the safety concept of THTR differs from those of other reactors. The decisive safety aims are:

Complete integration of the primary coolant circuits in the pre-stressed concrete pressure vessel

- hence limited loss of coolant in the event of credible accidents
- release of activity via ventilation stack in the maximum credible accident
- no sudden depressurization of core
- integrity of core structure

The penetrations in the concrete vessel are designed to prevent activity release by either

- two pressure-tight covers or
- an outer pressure-tight cover, and an inner flow limiting cover (about 33 cm² corresponding to a coolant pipe diameter of 65 mm) which, in the case of the outer cover being damaged, restricts the escaping coolant flow to a precise value.

Those primary coolant pipes which lead from the concrete pressure vessel to the auxiliary circuits are ≤ 65 mm diameter. These pipes are double walled as far as the fast action safety release valves. The safety container has no safety valves and is designed for the pressure of the maximum credible accident. The total primary building is divided into three categories of rooms, according to their different activity levels (Slide 12).

- Rooms continuously accessible during operation
- Rooms accessible only for a short time during operation, for inspection purposes
- Rooms not accessible during operation, access possible only after partial or total shut-down of plant

The third category includes primarily the room for the installation of the fuel circulating plant and part of the room for the gas purification plant as well as the space between air duct and pre-stressed concrete pressure vessel. According to their room categories, these rooms are separately ventilated and are connected to the ventilation stack via the normal exhaust air system and in addition, by a pressure safety system.

The safety analysis of this concept showed that, the maximum credible accident, with reference to endangering the surroundings, requires two independent faults to occur, namely

- a break in a primary coolant pipe (≤ 65 mm ϕ) outside the concrete pressure vessel
- the simultaneous malfunctioning of the safety release valve in that pipeline

In this case the escape of coolant gas is relatively slow, without a noticeable pressure rise in those rooms and can easily be vented. Even in the case of this maximum credible accident, the allowable contamination of the surroundings is not reached.

The largest realistic reactivity excursion through

- the accidental withdrawal of the two most effective control rods from the core

the largest realistic coolant disturbance by

- the simultaneous breakdown of three blowers, and the largest realistic pressure change through
 - the burst of a steam generator pipe and causing water ingress into the core
- have such a limited effect on the reactor, that the surroundings are not endangered.

THE STATUS OF THE 300 MWe THTR-PROTOTYPE

Construction documents for this prototype plant have been completed and an offer has been submitted. The licensing procedure has been initiated early 1970 (15/1). A utility group for the operation of this reactor plant has been founded and construction work is to be started in the beginning of 1971. The construction period is 5 years, including a test run of the plant of three month.

1 AVR Reactor Sectional View

2 THTR Section

3 Prestressed Concrete Pressure Vessel (Model) of THTR Power Plant with Steam Generators, Blowers, Core, Absorber Rod, Facility etc.

4 Graphite Structure-Ceiling

5 Graphite Structure-Bottom

6 Blower Section

7

Section of Core Rod Drive

8

Fuel Circulating System * (Flow Diagram)

*8 (Photo No.)

- 1- Fuel Element Charge Equipment
- 2- Burn-up Measurement
- 3- Switch
- 4- Pneumatic Elevating Device
- 5- Core

- 6- Singulizer
- 7- Damaged Sphere Separator
- 8- Damaged Sphere Container
- 9- Buffer-zone
- 10- Discharge of Spent Fuel
- 11- Process Computer

9

Fuel Circulating System - Building Block System

10

Gas shut-off Valve

11 Rotary Drive

12 Safety Concept

DISCUSSION

B. N. Furber: I would like to have further details of the metallic insulation. In the experience of TNPG this insulation suffers from at least two weaknesses: (1) It is highly anisotropic, (2) it is very difficult to design to accept interface pressure gradients. In the circulator outlet these two weaknesses are likely to seriously affect the insulation performance. What test work has been carried out in this area?

J. Schöning: We are aware of the problems raised by you and our people have a concept to overcome them, the details of which we are not able to present. There are extensive experiments and detailed design work going on to confirm the concept that has been selected.

R. D. Vaughan: The THTR reactor section shows the boilers to be of cylindrical plan form, but located in the pressure vessel vault along with the reactor core. May I conclude that the designers have looked at the alternative with pods in the wall of the pressure vessel and found it would be more expensive?

J. Schöning: The design of the THTR pressure vessel and the arrangement of the boilers within the vessel was already decided upon in 1965. At that time the pod-type vessel did not yet seem to be evaluated. At this time a change of the concept is not possible due to the advanced state of development and design. It is important, however, that the THTR-vessel allows the removal of the boilers in the same way as the pod-type vessel does. Within the development work for a large commercial power station a detailed comparison of the two vessels and their influence upon the plant is under way.

M. Dalle Donne: Coming back to the question of Dr. Furber, I think he answered his question himself with his paper. With helium one is in the region of Raleigh number where the Nusselt number is constant, so that a variation of helium velocity does not change the heat transfer coefficient of the insulation; although this may be the case with CO₂, where the heat is mainly transferred by convection, while with helium it is transferred by conduction.

THE LAYOUT OF THE CORE AND FUEL ELEMENTS OF THE THTR

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ABSTRACT

The continuous loading and circulation of the fuel elements in pebble bed reactors results in a number of performance differences from reactors with prismatic fuel elements. Their influence on the layout of the core and fuel elements of the THTR is described.

Introduction

The THTR is a high-temperature gas cooled pebble bed reactor, the construction of which is expected to be licensed within 1970. The THTR is operated on an uranium/thorium cycle, with fissile and fertile material mixed within each fuel element in a ratio of 1:10. The first batches of fuel elements discharged from the reactor will not undergo immediate reprocessing but will be stored for an unspecified period. Although an uranium/plutonium cycle operating at a lower enrichment would, under these conditions, result in equal or slightly lower fuel cycle costs, the uranium/thorium cycle has been selected because of its greater potential when reprocessing facilities will be available. Optimization calculations have resulted in the following design of fuel elements:

Content of fissile material (U235)	0.96 g/sphere
Content of thorium	9.62 g/sphere
Enrichment	0.93
Mean residence time in reactor	3.0 years
Mean fast neutron dose ($E \geq 0.1$ MeV)	$4.8 \cdot 10^{21}$ nvt
Maximum power per sphere	3.5 kW
Mean burn-up [fima]	0.12
Mean burn-up [fifa]	1.34
Mean number of circulations through core	6

The spherical fuel element has a diameter of 6 cm and consists of an exterior fuel-free graphite shell with a minimum thickness of 5 mm. Inside of this shell the coated particles are embedded in a graphite matrix of the same composition as the exterior shell.

Core Design

The initial core of the THTR consists of a mixture of approximately 350 000 of such fuel elements and an approximately equal number of graphite spheres, of the same diameter, containing no fuel. The same fuel element type is used for the initial core and the running-in period as well as for the equilibrium core, which allows a considerable reduction in the fuel element fabrication cost. The total uranium inventory of 359 kg U235 in the initial core is approximately equivalent to the sum of the contents of U235 and U233 in the equilibrium core. Because in the initial core, the thorium to uranium ratio is only half that of the equilibrium core and fission products have not yet been generated, approx. 40 000 absorber spheres must be added to the initial core, each containing 4 g of natural hafnium. Hence no trim rods are required for the initial core and the running-in period.

The core is subdivided in two radial zones. The central zone of 4 m dia is surrounded by an outer zone of 0.80 m width. In order to flatten the power and to obtain a sufficient reactivity worth of control rods inserted into the radial reflector, the ratio of fissile isotope density between the outer and inner zone is 1.5 and the burnable poison density ratio 0.66. This is achieved by mixing fuel, absorber, and graphite spheres in the two zones accordingly.

In this way a reasonable power flattening and a reactivity worth of 4.5% of 36 control rods in the radial reflector is obtained. Such a worth of the reflector rods is needed to compensate for reactivity changes during load-changing between 100 and 40 % (Xe-override) and for other adjustments.

The neutron flux increase in the outer zone of the reactor core required for this purpose is close to the economically optimal neutron flux distribution. Neutron leakage is increased only slightly. During steady load operation, a major number of these rods are inserted in the reflector to compensate for 1.7 to 2.6 % excess reactivity which must be available for controlling partial load operation and fine adjustment. If a daily load curve is to be followed, a few additional rods must be inserted or withdrawn for a few hours. The periods during which all reflector rods are either extracted or inserted are both very short.

When power operation starts the pebble bed is circulated. The spheres pass through the core in the downward direction within average transit time of six months. By the conical shape of the core bottom, the spheres are directed towards the central discharge tube, through which they are extracted from the core. Each sphere subsequently passes through a measuring equipment, by which the type of sphere - fuel element, graphite or absorber sphere -, and the burn-up state of the fuel elements can be determined.

Initially, some of the graphite and absorber spheres entering into the measuring equipment but no fuel elements are discharged from the system. An equal number of new fuel elements are added to the core to compensate fuel burn-up. The new fuel elements, together with the other spheres not discharged from the system, are lifted pneumatically through guiding tubes and drop on the pebble bed from a low height, thus closing the circulation loop.

The flow lines on which the spheres pass through the core, and the time required for passing through the core on different core radii have been experimentally determined in models of similar geometry. Specifically, it has been verified that the separation of the initial core into an inner and outer zone is maintained sufficiently distinct in spite of the circulation process. A mixing of the two zones which occurs in a region of two or three sphere diameters at the zone boundary is desirable, since steep gradients of power at the boundary are avoided.

In order to maintain the subdivision of the core into zones, three refuelling tubes point at the core center, and twelve at the outer zone. By an appropriate distribution of fresh and recirculated spheres to both of these tube systems any desired width of the core-zones can be established, but cones of different heights over both core-zones must be accepted.

With increasing burn-up the recirculated fuel elements, graphite and absorber spheres are distributed to the inner and outer zone of the core such as to maintain the power distribution of the initial core. After approximately one and a half years of full power operation - i.e., at a mean fima value of approximately 4.5 % - all graphite spheres will have been replaced by new fuel elements. At this time the overall thorium: uranium ratio has largely been shifted towards the equilibrium value, and fission products have accumulated to a considerable degree.

As power operation is continued, the first burnt fuel elements must be discharged from the core and replaced by new fuel elements. The burn-up measuring facility insures the discharge of fuel elements with highest burn-up; during the running-in phase these are the fuel elements of the initial core.

After a total of 3 years of full power operation the equilibrium phase in reactor fuelling is almost achieved. From then on the core composition and hence also the power distribution remains constant. Every day 621 new fuel elements must be added to compensate fuel burn-up.

To maintain the desired different fuel concentrations in both core zones during the equilibrium phase the fuel elements are divided into 3 classes, as determined by the measuring facility. Fuel elements having remained in the reactor more than 2.9 years of full power are discharged. Because of the different times for passing through the core at different radii this value corresponds to a mean residence time of the fuel elements of 3.0 years at full power. Fuel elements with a burn-up corresponding to a residence time between 1.6 and 2.9 years are subsequently added to the inner zone of the core. Fuel elements with a residence time of less than 1.6 years and all fresh elements are added at a ratio of 79 % to the outer zone and 21 % to the inner zone.

Figure 1 shows the residence time spectrum of the discharged fuel elements. Since it is required that only a negligibly small share of the burnt fuel elements (approx. 10^{-4}) remains in the reactor longer than 4.2 years of full power (corresponding to 14 % fima), the burn-up measuring facility must determine the uranium content of these elements to an accuracy of approx. ± 15 mg U235 equivalent. The fissile material content at discharge amounts to approx. 50 mg U235 and 250 mg U233. It has been confirmed by experiments that this condition can be met by measuring and evaluating the reactivity increase caused by the fuel elements passing through a measuring reactor of some hundred Watts of power. When the operation of the THTR or further irradiation tests permit an increase of the maximum residence time, the accuracy of burn-up measurement can be reduced.

The distribution of the fuel residence time round the mean value of three years is of small influence on the reactivity and hence on fuel cycle costs.

By recirculating the fuel elements with higher burn-up into the inner zone and by adding the new and the fuel elements of low burn-up preferably to the outer zone, the reactivity worth of the reflector rods of 4.5 Nile can be maintained also in the equilibrium core. Figure 2 shows the radial power distribution taken from a two-dimensional calculation at the core height of maximum power for completely withdrawn, completely inserted reflector rods, and for a rod position corresponding to full load operation. At full power operation leakage losses are 8.5 %. The maximum fluctuation of the local gas temperature caused by the shifting of the reflector rods are 200 - 250 °C, however with a gradient of less than 10 °C/min. The maximum fuel and gas temperatures have been calculated at 1250 °C, and 935 °C respectively.

The fuel temperatures in the THTR are favourably influenced by the mode of fuelling the reactor. Since the fuel elements are added from the top and part of their fissile material is burnt during their transit through the core, the power maximum is shifted towards the upper half of the core, that is towards the cold gas side.

Because of the continuous flow of the fuel elements the maximum temperatures in the fuel elements occur only during a short period, compared to their life time. The maximum temperatures occur at the gas stagnation area between the main coolant gas flow and the bypass which cools the element discharge tube. This area occupies approx. 10^{-4} of the core volume.

The fuel elements pass through this region within approx. 20 days. The maximum temperatures are generated in the fuel elements only during this time and only if all margins left for calculation errors on the reflector rod effectiveness sum up to the pessimistic side, which then requires insertion of all rods. During later transits through the core the fuel elements have already reached a partial burn-up so that lower fuel temperatures result after the first transit.

During operation no reactivities must be compensated other than the reactivity fluctuations resulting essentially from partial load operation, which are to be compensated by the reflector rods. Thus the 42 absorber rods located above the core are required for reactor shut-down exclusively, and can remain in withdrawn position during almost the entire power operation. Only for start-up after an extended shut-down, several absorber rods must remain inserted to compensate the positive reactivity due to the reduced xenon concentrations and to the decay of protactinium into uranium-233 during the shut-down period.

The requirement of the reactivity worth of these 42 rods must be calculated for the end of a shut-down period of several months. Then the protactinium has completely decayed into U233. A balance at that time is shown in Table 1.

Calculation Procedures

The calculation methods applied for the design of pebble bed reactors largely coincide with the procedures for other high temperature reactors. In addition to these standard methods computer programs were developed by which the unique method of charging pebble bed reactors can be taken into account.

The isotope distribution within the core, essential for calculating criticality, power distribution and control rod worth results only if the burn-up state and the local position of the individual fuel elements are known, which however depend on the neutron flux distribution, the flow behaviour of the spheres, and the charging strategy. Therefore, for calculating the equilibrium isotope distribution, a flux distribution must be assessed, and a charging strategy assumed.

For several fictive concentric flow channels, the increase of the fuel element burn-up is determined for each transit. By averaging the nuclide concentrations over appropriate volume fractions, an improved neutron flux distribution is calculated. If after several steps of iteration this distribution differs too much from the desired distribution, the charging strategy is varied.

The computer program which calculates this position-dependent core composition for a given charging strategy, takes into account the different transit times of the fuel elements in different flow channels, the influence of the burn-up measuring accuracy on the distribution of the recirculated fuel elements to the inner and outer core, and their final discharge from the reactor circuit¹. The 2-dimensional flux calculations in r, z -geometry must be carried out with a rather fine subdivision of the core volume into material zones, in order to represent the flow paths of the elements sufficiently.

During the running-in period the core composition and the power distribution are time dependent. Therefore, burn-up calculations are carried out starting with the power distribution of the initial core. The core is subdivided into a number of sphere packages. Using the neutron flux averaged over such a sphere package, the burn-up in the fuel elements of this package is calculated for a time increment with the assumption that the circulation movement can be neglected. Subsequently the individual sphere packages are shifted according to the paths advanced due to their movement during this time increment. The calculation of the further increase in burn-up is then performed with the neutron flux at the new position of each sphere package. After a number of time increments, the flux distribution must be recalculated. A difficult data storage problem within this procedure is the follow-up of the nuclide concentration in those sphere packages which are recirculated after a transit through the core and where individual spheres are either assigned to different core zones or partly discharged. The nuclide concentrations must be distributed accordingly.

The temperature distribution is also calculated in r, z -geometry. The geometry is described by a mesh layout. The mass flow distribution is determined by similar methods as developed in electrical network theory. The mass flow and temperature dependence of the flow resistance is taken into account.

For determining the release of fission products the temperature and burn-up history of one representative fuel element during its residence time is calculated. As transport mechanism of solid fission products, temperature-activated solid-state diffusion and evaporation from the surface of the fuel element into the coolant gas are considered. The time-dependent diffusion equation is solved numerically under variable temperature and power conditions for the fuel particles, the fuel coating, the graphite matrix, and the fuel-free shell of the fuel element. The contamination of the coating and the matrix by fission material during fabrication and the diffusion of inert gas predecessors for various radioactive decay chains are taken into account.

The results are time-dependent release rates of one representative element and - by integration over the core volume and time - the total release of the core.

With similar procedures corrosion rates and stresses are calculated, the latter program makes use of an existing computer code "STRETCH", adapted to the requirements of a moving fuel bed.

Consequences for the Fuel Elements Layout

The major part of activity in the primary circuit is due to gaseous fission products caused by the contamination of the fuel elements with fissile material, either directly or indirectly by decay of a gaseous predecessor.

The favourable influence of the moving fuel on the total release from the core can be demonstrated by comparing a static core with a continuously flowing core of identical temperature distribution and residence times.

The release rates of long-lived fission products from the moving fuel elements are far below those of the static core; for strontium 90 e.g. they are lower by approx. 3 orders of magnitude. Therefore, even without having a secondary containment around the primary circuit no layer of SiC is required in the coating of particles. This is important for improving the neutron economy, and lowering both fabrication and reprocessing costs.

Since the fuel elements will remain within the region of high temperatures for relatively short periods only, no stringent requirements are needed for the irradiation resistance of the graphite. The graphite selected for the fuel elements (see Figure 3) starts expanding at temperatures of approx. 900 °C and doses of 8×10^{21} nvt ($E \geq 0.1$ MeV). This has to be compared with the maximum dose of 6.3×10^{21} nvt in the reactor, which is only reached by a fraction of 10^{-4} of all elements discharged.

The results of stress analysis for the fuel elements in reactor operation are shown in Figure 4. The maximum tensile stresses occur after the first transit through the core, since then the temperature gradient disappears because of the sharp reduction of power production. In the core the temperature gradient partially compensates the stresses caused by dimensional changes due to irradiation. The sudden change of tension decreases with increasing burn-up. A comparison with calculated stresses of irradiation experiments shows (see Figure 5) that the maximum stresses occurring in these experiments are much higher than those to be expected in the THTR. Therefore, there will be no damage of the fuel elements by irradiation-induced and thermal stresses.

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Table 1. Reactivity Balance

Requirement

1.7	Nile	Xe-override between full and 40 % load (normally compensated by reflector rods)
2.0	Nile	Temperature
3.8	Nile	Xe-equilibrium
3.6	Nile	Pa-233 decay and U233 build up
0.5	Nile	Margin to compensate fluctuations due to deviations from refuelling plan
<hr/>		
11.6 + 1.1		Total

Rod efficiency

17.3	Nile	Calculated worth of 42 rods
1.3	Nile	Worth of the two most efficient rods
<hr/>		
16.0 + 2		

4.4 ⁺ 3.1 Shut-down margin

Table 2. Fission Product Release into Primary Circuit

Isotope	moving core		static core	
	from impurities	from kernel	from impurities	from kernel
Xe 133	1300 Ci	-	1300 Ci	-
Xe 135	150 Ci	-	150 Ci	-
Kr 88	1350 Ci	-	1350 Ci	-
Kr 85m	750 Ci	-	750 Ci	-
J 131	10 ⁻² Ci	-	50 Ci	-
J 132	10 ⁻² Ci	-	10 ⁻² Ci	-
J 133	10 ⁻² Ci	-	0.1 Ci	-
Sr 90	-	70 Ci	-	ca. 10 ⁵ Ci
Cs 137	260 Ci	-	1500 Ci	8-10000 Ci

Fig. 1. THTR Fuel Element Residence Time Distribution Function.

Fig. 2. Radial Power Distribution in THTR Equilibrium Core.

Fig. 3. Dimensional Changes of THTR Fuel Element Graphite.

Fig. 4. Reference Fuel Element: Tangential Stresses and Power in THTR.

Fig. 5. Reference Fuel Element: Tangential Stresses and Power in Irradiation Experiment.

DISCUSSION

J. D. Thorn: What is the design value of R/B?

H. Harder: 1×10^{-4} .

L. W. Graham: In looking at the stresses in the ball, have you considered the effects of the differential dimensional changes between the fuel and unfueled parts?

H. Harder: No. The stresses shown in the slide concern a sphere of homogeneous graphite matrix.

J. D. Thorn: What changes in heat transfer are expected around the fuel element ball?

H. Harder: Not significant.

F. P. O. Ashworth: There is a complex vector field of ball velocities and swell times in the core. Is there a probability of a ball staying in the core long enough for the release to exceed the tolerable core total for a particular fission product?

H. Harder: There is only an extremely small probability that spheres remain in the core for such a long time. Even then this would result in a negligible effect on total activity of the primary coolant.

T. A. Jaeger: This question concerns the diagram showing the results of a thermoviscoelastic analysis of the spherical fuel element. Could you give some details on the underlying assumptions and the method of analysis utilized, e.g., did you include considerations on the affects of contact pressures and local heat transfer disturbances?

H. Harder: The temperature distribution due to the power produced in a fuel element during its several transits through the core and the stresses resulting from the time-dependent temperature gradients in the sphere have been calculated. The stresses due to irradiation affects have been determined by piecewise evaluation of the curves representing shrinkage of graphite versus neutron dose as measured in irradiation experiments at the corresponding temperatures. This includes some approximation because the temperature in each of these experiments has been

confined during the whole irradiation times. For creep, proportionality between $d/dt (\Delta l/l)$ and stress has been assumed. The effects of contact pressures and local heat transfer disturbances have not been considered in this calculation.

W. R. Martin: What is the absorber material to be used in the 42 control rods?

H. Harder: B_4C .

W. R. Martin: How is the Hf and B being incorporated into the fuel elements as a burnable poison?

H. Harder: The burnable poisons will not be incorporated into the fuel elements, but will be contained in absorber spheres, which are free of fuel.

W. P. Barthold: How well do you measure the burnup and what is the peak-to-average burnup ratio?

H. Harder: The burnup can be measured with an accuracy of 10-12 mg U^{235} equivalent. This has been demonstrated in a prototype of the measuring reactor. The maximum burnup in the equilibrium core is 14% fima, the mean burnup 12% fima.

B. G. Chapman: Was the figure of 2.0 nils reactivity change for temperature, the full value from full power to a cold core, or to a warm core with decay heat after shutdown?

H. Harder: 2.0 nils is the reactivity change from full power to a cold core. This figure concerns the equilibrium core. In the initial core, the corresponding value is about 4.0 nils.

L. A. Lys: Could you describe the design principal of the control rod, in particular their mode of operation for reactor scram.

H. Harder: The control rods are inserted into the pebble bed without guiding tubes. It has been described by Schöning in his paper. It has been demonstrated by a series of experiments in models of similar geometry that such a procedure is possible without having too high loads

J. D. Hart: What is the design limit for corrosion of the fuel spheres? Should this level of corrosion occur in practice, is it expected that this would cause dust to be generated?

H. Harder: The exact specification of the corrosion rate will be given by Dr. Hackstein in his paper. Concerning dust, we can rely on the AVR experience, where only negligible amounts of dust have been found up to now.

G. Ivens: Because of the large amounts of chemical impurities in the AVR primary circuit, we should have expected considerable corrosion and dimensional changes. In fact, we have found no evidence of such changes in fuel spheres taken from the AVR core.

A. Chamberlain: In paper 1/101 Dr. Krämer referred to extensive guarantees on THTR fuel from the fuel manufacturer. This presumably implies that the irradiated fuel elements are monitored for fission product release before being fed back into the reactor. In this context what constitutes a fuel element which has not met the guarantees?

H. Krämer: The THTR has divided for measuring the burnup of each ball leaving the core; and it also has a device for measuring the fission gas release of each ball. Balls having a burnup less than 8% fima and a fission gas release of more than 5×10^{-4} R/B will be replaced under the guarantee given by Nukem.

LARGE HTGR DESIGN STATUS

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ABSTRACT

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The design status of a nuclear steam supply system employing a high-temperature gas-cooled reactor and capable of producing sufficient high-quality steam to generate approximately 1100 MW(e) in a conventional reheat steam cycle is discussed. This design--the result of more than two years of engineering effort by a large fraction of the design engineering staff of Gulf General Atomic Incorporated--is based on the same technology and development work as the 330-MW(e) Fort St. Vrain Nuclear Generating Station which Gulf General Atomic is presently constructing for the Public Service Company of Colorado under the United States Atomic Energy Commission Power Reactor Demonstration Program. Several design improvements have been incorporated which either improve the safety or economics or facilitate the operation and maintenance of the plant. The reasons for some of these improvements are summarized.

INTRODUCTION

The large high-temperature gas-cooled reactor (HTGR) presently being offered commercially by Gulf General Atomic Incorporated is the result of over a decade of development in the basic technology of gas-cooled reactors and in the design and proof-testing of the components which will be used in the plant to generate high-quality steam for electrical power generation. The experience gained from the operation of the Peach Bottom (40-MW(e)) plant and from the design, licensing, and construction of the

Fort St. Vrain (330-MW(e)) plant has provided the guidance for system and component design for the large HTGR nuclear steam supply system. Moreover, preliminary discussions have been held with the United States licensing authorities on the design of the plant. Their review has been helpful in establishing the design bases for the engineered safety features and in providing assurance that the design can be successfully engineered and licensed and safely operated. The use of a conventional secondary containment building and auxiliary cooling loops, as a result of the discussions with the licensing authorities, is expected to be a major factor in easing licensing difficulties.

NUCLEAR STEAM SUPPLY DESCRIPTION

The HTGR nuclear steam supply system (NSS) is presently designed to produce main superheated steam at 955°F and 2400 psig and reheat steam at 1001°F and 575 psig in sufficient quantities to generate 1100 MW(e) in a conventional reheat steam turbine-generator unit. Figure 1 is a schematic flow diagram for the plant. Helium at a pressure of 685 psig is circulated by means of steam-turbine-driven axial compressors downward through the reactor core and upward through the once-through steam generators. Six loops are used, each loop having a single helium circulator unit and a single steam generator unit. The main superheated steam passes to the high-pressure element of the turbine. The high-pressure turbine exhaust is used to drive the helium circulators before passing first to the reheat sections of the steam generators and then to the intermediate-pressure and low-pressure sections of the main turbine.

The primary coolant helium is heated from 630° to 1377°F mixed mean in passing through the reactor core. The pressure drop in the entire primary coolant circuit is approximately 15.25 psi, about half of which takes place through the reactor core.

Figure 2 shows the general arrangement of the major components of the NSS within the prestressed concrete reactor vessel (PCRV). The reactor core, reflector, control rods, and internal structures occupy the central cavity, which is about 37 ft in diameter by 44 ft high. The six separate main cooling loops occupy 13-ft-diameter cavities which are spaced symmetrically around the central cavity and connected to it by insulated radial ducts. The PCRV is a right cylinder, 81 ft high by 96 ft 6 in. in diameter. Process and service connections to the steam generator units are made through penetrations at the bottom of the PCRV, and connections to the circulators are made at the top. Control rod drive assemblies are mounted in the 73 refueling penetrations in the top head, where additional penetrations are also provided for helium purification system components and nuclear instruments. Three additional cavities approximately 7 ft in diameter with penetrations in the top head are provided for the auxiliary circulators and heat exchangers which are used to satisfy the emergency cooling requirements for the reactor.

Prestressed Concrete Reactor Vessel

The PCRV functions as the primary coolant system boundary. The complete PCRV consists of (1) an internal steel liner which acts as a sealing membrane, (2) a thermal barrier mounted on the inside of the liner and a cooling coil system attached to the outside of the liner whose joint function is to limit the temperature of the concrete, (3) penetrations and closures which provide access to the vessel cavities, and (4) high-strength concrete with bonded reinforcement steel and a prestressing system to provide strength. The entire assembly is mounted on a support structure anchored to the foundation pad.

The PCRV for the large HTGR has a different configuration than the Fort St. Vrain vessel. The multicavity design and the radial location of the steam generators and circulators are design improvements which result in better loop separation and an improved capability for the removal, if necessary, of the steam generators and circulators for maintenance and repair. The multicavity design also results in a shorter PCRV, which yields

an improved capability to withstand accelerations due to earthquakes.

The large HTGR incorporates single closures on the PCRV penetrations with flow restriction devices in order to limit the rate of depressurization of the PCRV for the hypothetical closure failure accident. The structurally separate containment building provides the secondary containment for the large HTGR, whereas the containment for the Fort St. Vrain plant is provided by secondary closures on every penetration. This particular design change resulted from discussions with the licensing authorities which led to in the requirement to consider the double closure failure as a design basis accident. It thus became apparent that the control of radioactivity releases to the site boundary under the assumed accident conditions could be accomplished more economically if a secondary containment structure was added.

The PCRV itself is very similar to the Hartlepool vessel being constructed by Taylor Woodrow Construction in Great Britain. Linear tendons are used for vertical prestress and wire winding is used for the circumferential prestress. Work sponsored by Gulf General Atomic is under way to develop and proof-test the machinery required to perform the wire winding operation. The use of wire winding represents a significant improvement over the linear circumferential prestress in that considerable relief is obtained in embedment congestion and in the complexities associated with pilasters for the circumferential prestress anchors. Moreover, the present design does not incorporate head tendons, since sufficient prestress to the top and bottom heads of the vessel can be applied by the circumferential prestressing operation.

Since both the steam generator and the circulator are installed through the same penetration, a concrete plug is used in the upper end of each steam generator cavity as a means of reducing the penetration diameter to that required for the circulator after the steam generator has been installed. These plugs are bolted to the top of the steam generator penetration liners and have a central hole through which the circulator assembly is installed.

Steam Generators

The once-through steam generators for the large HTGR are generally larger versions of the Fort St. Vrain modules. Each of the six units is approximately a factor of six larger in weight and heat output per module. Figure 3 is a schematic diagram of an installed unit. Helium enters the unit at the bottom and flows upward through a U-tube reheater located in the center duct. The flow is reversed at the top of the unit, and the helium then flows downward over the helically coiled superheater-evaporator-economizer bundles. At the bottom of the unit, the flow is again reversed, and the helium flows upward in the annular region between the steam generator shroud and the cover plates on the insulation for the steam generator cavity liner. Uphill-boiling in the main steam section has been retained in the design, and the difference between the large HTGR steam generator and the smaller Fort St. Vrain module configurations is the direct result of this design decision. Downhill-boiling was considered in earlier conceptual designs but was not used owing to concern over flow stability at low power.

A significant design simplification was made in the method of headering the feedwater and steam tubes at the steam generator penetration. The Fort St. Vrain steam generators have a domed primary closure. Feedwater and superheater subheaders pass through this closure, are coiled through the interspace, and then pass through the secondary closure to ringheaders mounted outside of the penetration. Reheat steam inlet and outlet are through a concentric pipe arrangement in the center of each closure. The large HTGR uses tubesheets for headering the feedwater and all of the steam tubes. These tubesheets form part of the closure for the steam generator penetration. In addition to the helium flow restrictors in each penetration which provide the flow restriction necessary to limit the hypothetical PCRVR depressurization accident, flow restrictors are also provided to limit the ingress of steam/water into the reactor for a hypothetical tubesheet failure.

Direct access for tube plugging operations is provided by this design simplification. These operations would be accomplished during a plant shutdown by removing sections of the piping below the tubesheets, manually plugging the leaking circuits, and then replacing the pipe sections.

The materials used in the steam generators range from carbon steel to nickel-base alloy. Carbon steel is used in the low-design-temperature items such as the feedwater tubesheet and the feedwater tubes. Various grades of low-alloy steel are used for the closure, the cold reheat tubesheet, portions of the shroud, and the economizer-evaporator tubes and their support structure. Nickel iron-chrome alloy (Incoloy 800 or equivalent) is used for the superheater tubes, the reheater tubes, and portions of the shroud, and a high-strength nickel-base alloy (Inconel 625 or equivalent) is used for the superheater tubesheet, the hot reheat tubesheet, and the support structures for the superheater and reheater.

Main Helium Circulators

The main helium circulators (shown in Fig. 4) are generally larger versions of the Fort St. Vrain series-steam-turbine driven axial flow units, with flow and power requirements approximately double those of each of the Fort St. Vrain circulators. The thrust and journal bearings are water-lubricated hydrostatic bearings with hydrodynamic capability at high speed, similar to those in the Fort St. Vrain units. Owing to the reactor arrangement, the units are inverted relative to the Fort St. Vrain circulators and the helium flow through the compressor is reversed, but these are minor design modifications. A shutoff valve is located at the discharge of each circulator to prevent backflow of helium through a shutdown circulator.

Control Rod Drives

Figure 5 shows a control rod drive unit installed in a refueling penetration. These units, which are very similar to the Fort St. Vrain units, are essentially electrically powered winches, each of which raises and lowers a pair of control rods by means of flexible steel cables. The

control rods are guided into the core by a guide tube assembly located in the upper plenum above the reactor core. Scram occurs by gravity action, with the velocity being controlled by regenerative braking action of the motor.

Fuel Handling Equipment

Refueling operations will be carried out with the plant off-line and with the reactor helium inlet temperature and pressure lower than 250°F and atmospheric, respectively. The principal equipment (shown in Fig. 6) consists of a fuel handling machine for actually removing and replacing fuel and reflector elements in the reactor; fuel transfer casks for transferring canned elements between the fuel handling machine and storage; an auxiliary service cask for removing and installing control rod drive assemblies before and after refueling; reactor isolation valves to provide shielding and sealing for the refueling penetrations during the refueling operation; and motorized vehicles for transporting the fuel transfer casks between the fuel handling machine and storage.

The in-core operations are identical to those for the Fort St. Vrain reactor; i.e., a complete seven column refueling region is refueled at any one time. However, the Fort St. Vrain refueling machine itself temporarily stores the fuel and reflector elements and is moved to the location of the fuel storage to deposit the spent fuel and reload with new fuel. This type of operation was prohibited in the conventionally contained plant unless a very large access door was provided between the containment and the reactor service building. Moreover, it was found that the refueling time could be shortened considerably if the refueling machine was not moved and fuel was transported by the transfer casks and motorized vehicles.

The refueling cycle is based on a 4-yr life; i.e., one-quarter of the core is replaced each year by new or recycled fuel. The reactor shutdown time required for the refueling operation is estimated to be about 14 days, including the time required for pump-down and cool-down of the reactor and the time required for pump-up and reactor startup.

Reactor Core

The reactor core (shown in Fig. 7) consists of approximately 3800 hexagonal graphite block fuel elements of the same basic type as will be used in the Fort St. Vrain plant; i.e., element overall dimensions are approximately 31 in. long and 14 in. across the flats. The graphite fuel elements are arranged in columns eight blocks high and are surrounded by a graphite reflector approximately 4 ft thick. The core itself is divided into 73 regions, each consisting of a central fuel element column surrounded by up to six columns. Each central fuel element column contains the vertical holes for the associated control rod pair and a hole for the insertion of the reserve shutdown material.

Conventional nuclear-grade graphite will be used throughout the core. The fuel materials in the initial core will be 93% enriched uranium and fertile thorium in carbide form. In the later recycle mode, U-233 will be included as a feed material, replacing much of the U-235. The uranium and thorium carbide particles are coated with layers of pyrolytic carbon and bonded into rods before being loaded into the graphite elements.

Apart from differences in loadings, the major difference between the large HTGR fuel and Fort St. Vrain fuel is in the coatings. In order to minimize the accumulation of Sr-90 as plateout in the circuit, which would otherwise be potentially releasable in the hypothetical depressurization accident, all the Fort St. Vrain fuel particles have an added layer of silicon carbide. The addition of conventional containment on the large HTGR plant has relieved this release problem. However, in the large HTGR certain U-235 fuel particles also have a silicon carbide coating to facilitate separation during reprocessing operations.

Auxiliary Cooling Loops

Three auxiliary cooling loops have been provided in the NSS for the large HTGR. Each loop consists of a helium circulator, a water-cooled

heat exchanger, and their associated service equipment. Figure 8 shows one of the loops installed in the PCRV cavity. The primary function of the loops is to provide emergency core cooling and decay heat removal in case of failure of the main loops. Any two loops can cool the core.

The requirement for these auxiliary cooling loops was a consequence of discussions with the licensing authorities and technical difficulties in providing adequate static cooling for the large HTGR core in a manner similar to that provided for the Fort St. Vrain core. Failure of both the steam drives and the water turbine drives on all the Fort St. Vrain circulators results in the hypothetical loss of forced circulation accident with the decay heat being removed by radiation and conduction to the liner cooling system. Although high temperatures are reached in the core of the Fort St. Vrain reactor during the course of this hypothetical event, the reactor can ride through the long term heatup and cooldown without violating the containment provided by the PCRV. However, removal of afterheat by the liner cooling system of the large HTGR is more difficult simply because of the longer radiation and conduction paths. Higher temperatures would be attained if a similar loss of all forced circulation accident is hypothesized. Attempts to assure safety by some means of static core cooling did not prove satisfactory, and as a result an additional method of providing forced cooling was adopted.

Each auxiliary helium circulator is a fixed geometry axial-flow compressor driven by a variable-speed electric motor to cover the full range of possible operating conditions, including a core cavity pressure anywhere between the full inventory pressure and atmospheric. Oil-lubricated ball bearings are utilized. A shutoff valve in the inlet duct of each auxiliary circulator prevents significant backflow through the auxiliary loops when the main circulators are operating.

Each auxiliary heat exchanger is a vertical U-tube design supported from above, permitting free axial expansion of the tubes.

ENGINEERED SAFEGUARDS AND SAFETY PHILOSOPHY

The evolution of the large HTGR design has resulted in two major changes in the engineered safeguards systems:

1. The addition of a separate conventional containment building to provide greater physical separation between the primary and secondary containments.
2. The incorporation of auxiliary cooling loops to provide emergency core cooling in case of failure of the main circulator/heat removal system.

As noted previously, the acceptance of a hypothetical depressurization of the PCRV as a design basis was the primary reason for the addition of the separate containment building. In order to provide an acceptable upper limit for the rate of depressurization, the hypothetical accident is assumed to occur through a penetration in the PCRV after failure of the closure, with the subsequent depressurization rate being determined by the free-flow area around a flow restrictor in the penetration. The penetration flow restrictors are engineered safety features and have the capability of limiting the effective free-flow area to a maximum of 100 in.² Figure 9 shows the temperature and pressure pulse in the containment which would occur during this event. Since the energy content of the single-phase helium coolant is so small, a separate heat removal system in the containment is not necessary; the heat content of the helium is removed by transfer to the cooler surfaces of the containment and its contents.

The function of the auxiliary cooling loops, which are also engineered safety features, is to provide adequate removal of the decay heat from the core following the design basis depressurization accident. The main cooling loops are not considered to be engineered safety features; however, they also have the capability of removing the decay heat and adequately cooling the reactor core.

The evolution of the safety philosophy for the NSS has also led to consideration of the possibility of some additional input to the design basis accident from small leaks in the secondary coolant system. It is becoming common practice to consider suitable design margins for such an event in the licensing of power reactors. For the HTGR, the possibility of steam/water ingress into the reactor coincident with or subsequent to the depressurization accident does require the additional engineered safety features. Until all sections of the reactor core are cooled to below the threshold for the steam-graphite chemical reaction, some hydrogen and carbon monoxide will be generated. It is necessary to provide adequate monitoring for moisture in the system and suitable isolation devices to limit the amount of steam/water ingress to less than that which could lead to potentially flammable mixtures of hydrogen and carbon monoxide in the air-filled containment. The moisture monitoring devices and the isolation systems for sources of large steam/water leaks have therefore also been classed as engineered safety features for the large HTGR.

SUMMARY

This paper has covered only the highlights of the NSS for the large HTGR. Sufficient design and development work has been done on the design for Gulf General Atomic to now offer the nuclear steam supply for the generation of base load electrical power in 1100-MW(e) units on a commercial basis. The HTGR's potential for improved performance in terms of higher plant efficiency, reduced radioactive and thermal effluent, and less stringent fuel requirements is rapidly approaching realization in the 1100-MW(e) design.

Figure 10 shows an overall view of the 1100-MW(e) HTGR power plant. Design criteria have been established for the major systems and components in the plant, and work on the detailed design is proceeding. The secondary containment structure surrounding the PCRV is a carbon-steel-lined conventionally reinforced concrete structure with cylindrical walls, a flat base, and a hemispherical dome. Its overall height is about 230 ft and its inside diameter about 126 ft. A minimum clearance of approximately 8 ft is maintained

around the PCRV to permit post-tensioning of the circumferential wire wrapping. Access to the containment building is provided by a 7-ft-diameter personnel hatch and a 22-ft-diameter equipment hatch.

The reactor service building contains the new and used fuel storage facilities and the reactor auxiliary systems and is approximately 90 ft wide, 130 ft long, and 172 ft high. The three-story turbine building is approximately 136 ft wide, 300 ft long, and 102 ft high.

Fig. 1. Schematic Flow Diagram of NSS for Large HTGR.

Fig. 2. General Arrangement of Large HTGR.

Fig. 3. Steam Generator Installation.

Fig. 4. Helium Circulator Installation Assembly.

Fig. 5. Control Rod Drive and Reserve Shutdown Assembly.

Fig. 6. Fuel Handling Equipment.

Fig. 7. Core Arrangement.

Fig. 8. Auxiliary Cooling Loops.

Fig. 9. Containment Pressure and Temperature Transients during Design Basis PCRV Accident in 1100-MW(e) HTGR with Concurrent Steam Leak.

Fig. 10. Overall View of 1100-MW(e) HTGR Power Plant.

DISCUSSION

D. Tytgat: What led you to the choice of a secondary containment vessel? Was it the unreliability of the secondary closures of the large penetrations or the general safety philosophy adopted in the USA for large water reactors?

A. J. Goodjohn: After several discussions with licensing authorities, it became apparent that a rapid depressurization of the PCRV could be considered as a design basis accident for the concept. Moreover, it also became apparent that the general public would raise several concerns over a plant without the secondary containment independent of any safety arguments on the very low probability of PCRV failure. Therefore, GGA adopted secondary containment by means of a conventional containment building in order to satisfy the general safety philosophy adopted in the United States and not because of any concern over the reliability of the secondary closures on the large penetrations.

G. Meijer: One of the major differences between the GGA design for a 1100 Mwe HTGR and the TNPG HTGR design is that you are designing for off-load refueling and TNPG for on-load refueling. Therefore, there appears to be no particular reason for having a downflow in the GGA reactor. It appears that with an upflow in the core the steam generator design would have been simplified, i.e., cheaper — and also the core support would have been cheaper. Therefore, I am interested to learn what made you decide on a downward cooled core; is this because of pressure drop or because of other reasons?

A. J. Goodjohn: We decided to stay with downflow through the core for the following reasons: A. The possibility of going to on-line refueling on a future plant improvement, B. Fort St. Vrain has downflow and the development of the control rod drives and the thermal insulation have been based on this arrangement. We would have to develop a control rod drive for hot helium (cable problem) and redo much of our insulation work.

D. Patterson: You assume a 100 sq in. opening for the design basic accident. What determined the size of this assumed failure?

A. J. Goodjohn: The 100 sq in. opening in a penetration as a design basis for the hypothetical depressurization accident is greater than the size of opening which one obtains by design of flow restrictor devices (approximately 30 to 50 sq in. for large penetrations) and smaller than the size of the opening which could result in secondary effects in the primary circuit; i.e., failure of internal seals or duct work. The licensing authorities agreed with this design basis for Fort St. Vrain.

T. A. Jaeger: My question refers to the design of the concrete pressure vessel. You stated that the circumferential wire winding prestressing provides adequate prestressing of the vessel cap. In this way the cap is taking the load entirely by arching action which may imply a sudden mode of failure. What is your philosophy with regard to safety margins for the design of the cap as compared with the cylindrical wall?

A. J. Goodjohn: By applying sufficient circumferential prestress to the head one can assure sufficient strength in the head to yield as great a safety margin against overpressure blowout of the head as against flexural failure of the side wall. One, in fact, designs the prestress so that ultimate failure mode at very high pressures is predictable and is not head failure or side wall failure but failure at the haunch between the head and the side wall. The model tests have proven this concept.

R. E. Helms: Most large fossil plants in the U.S. use the supercritical steam cycle 3500 psia/1000°F/1000°F. What was GGA's basis for selecting the subcritical steam cycle 2400 psia/1000°F/1000°F? What trade-off were made, if any?

A. J. Goodjohn: GGA did evaluate the supercritical steam cycle for the large HTGR and decided on an economic basis primarily to remain with the subcriticals 2400 psia/1000°F/1000°F cycle.

CONCEPTUAL DESIGN FOR A 600 MW_e NUCLEAR POWER
PLANT WITH HIGH TEMPERATURE REACTOR AND HELIUM
TURBINE ⁺⁾

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ABSTRACT

In the Federal Republic of Germany a development program for a 600 MW_e High Temperature Reactor Power Plant with closed gas turbine cycle has been initiated. The paper describes the present state of the conceptual design studies.

The activities are concentrated on the layout of the complete plant, development of fuel elements for high temperatures with low fission product release rates, problems of deposition of fission products, and development and testing of special components.

The decision concerning the layout of the main components will be made in 1970, a preliminary decision on the fuel element type in 1972. By 1975 complete construction documents will be available.

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- +) The work has been carried out within a cooperation contract between the corporations Brown Boveri/Krupp Reaktorbau GmbH, Brown, Boveri & Cie AG, Fried. Krupp GmbH, Gutehoffnungshütte Sterkrade AG, Maschinenfabrik Augsburg-Nürnberg AG, NUKEM, Nuklear-Chemie und -Metallurgie GmbH and Kernforschungsanlage Jülich GmbH, regarding the preparation of the complete construction documents for a 600 MWe power plant with HTR and helium turbine, under the sponsorship of the Federal Ministry for Scientific Research (Inv. Reactor 65).

INTRODUCTION

The rapid progress in the development of fuel elements for high temperature reactors in recent years has provided the technical basis for the exploitation of the high efficiency of this reactor type, by coupling it directly with a helium turbine. Other advantages associated with direct coupling, such as lower specific capital cost, lower consumption of coolant water and high efficiency even in partial load operation, help to ensure that the high temperature reactor with helium turbine will, in the long run, take up a favourable position alongside fast breeders in the energy production industry.

In the Federal Republic of Germany a program has been initiated for the development of a 600 MW_e plant. The program is being carried out by several companies in co-operation with the Kernforschungsanlage Jülich GmbH (KFA). For experimentation and development up to the stage at which an offer can be presented, approximately 130 Mio DM are available. The activities are concentrated on the layout of the complete plant, development of fuel elements for high temperatures with low fission product release rates, problems of deposition of fission products, and development and testing of special components. Use of spherical and rod-type fuel elements are currently being investigated in parallel; however, final design for construction is to be carried out for one fuel element type only.

DESIGN ASPECTS

The aim of the design work is to establish an optimum between high efficiency and low capital cost, for a plant to be built within a few years.

In Fig. 1 the basic data for the plant design are listed, showing a set of data A for the minimum operating cost and a set B for the minimum cost.

- The maximum process temperature (gas turbine inlet) of 850°C was selected as a realistic value with regard to the permissible fuel element load, release of fission products and use of available materials. A temperature increase can be envisaged resulting in a high development potential since it can be directly exploited with the application of gas turbines. A temperature increase by 50°C results for instance in an increase of total efficiency by approximately 2 points and will probably allow an estimated reduction in capital cost by 20 DM/kW installed power.
- To achieve a maximum possible site independence air cooling has been selected. Under the assumption that an intermediate cooling circuit is required for safety reasons the minimum process temperature has been chosen to 35°C . A possible lowering of this temperature down to approximately 22°C by using river water for recooling would result in an increase of total efficiency by approximately 2 points, which corresponds to an increase of turbine inlet temperature by 50°C .
- A simple intercooling was favoured to reduce the building volume, and machinery costs. Although double intermediate cooling results in an increase in efficiency by approximately 1 1/2 points, the overall economics of the power plant however, becomes less favourable because of the higher capital cost.
- For the choice of maximum pressure level - in this case approximately 60 bar - a compromise had to be reached between plant size and costs on the one hand and efficiency of the turbo machinery on the other hand.

- The investigations of this project are based on the use of a single-shaft horizontally-arranged gas turbine plant. A multi-shaft arrangement has been excluded due to the fact that it results in higher capital costs, has a more complicated layout and requires a larger control system. Thus for example turbine compressor units with vertical shaft could be developed only at considerable additional expenses.
- The net efficiency of the power plant of 42 % was determined on the basis of these basic data by optimization of the turbine expansion ratio over the reactor inlet temperature, the pressure losses, and the heat exchanger temperature differential. With double intercooling and recooling by river water, plant efficiency is increased to 45,5 %. The overall economics, however remain approximately unchanged because of the increase of capital cost.

SAFETY ASPECTS

The essential safety requirements have been determined under the following considerations:

- possibility to shut down the reactor
- removal of decay heat
- radiation dose in normal operation and in fault conditions.

The most serious problem regarding shut-down of the reactor is the "loss of coolant" accident, caused by rupture of a pipe in the main circuit. Due to the relatively large pipe diameters in the main gas circuit and the high velocity of sound in helium the rupture of a pipe results in a very rapid depressurization of the circuit and the core. The same applies, though to a lesser extent, to an equalization of pressure within the system, caused by an accident, without release of coolant gas to the environment. Because of the

possibility that a rapid depressurization may occur, special design and layout are necessary for the core structure to prevent damage to the core and the related structure so that reactor shut-down is not impaired. The same applies to the removal of decay heat.

The decay heat removal system must operate under various pressure conditions, between full design pressure and approximate ambient pressure. Therefore stringent requirements result for the design of this system and for the control of the blower power. Special attention must be attached to the problem of thermal shocks.

The radiation exposure of the environment and within the power plant during normal operation and under fault conditions does not create any insurmountable problems for the equilibrium coolant gas activity expected.

Based on experience, the permissible normal leakage will not be exceeded, even with a shaft penetration through the PCPV and a great number of flanges to be sealed.

- The release of the total coolant is handled, from the safety stand point, by a controlled release through a ventilation stack.
- There are some uncertainties in maintaining sufficiently low radiation doses during inspection and repair work within the area of the main circuit, since at the present date insufficient information on contamination mechanisms is available. Favourable experience with the DRAGON and the AVR reactor indicate, however, that also for this problem acceptable solutions can be expected.

LAYOUTS

In the development phase a great number of different designs and layouts are possible which appear equivalent. Therefore a systematic evaluation is required. According to recent consideration, this evaluation can be reduced to a few principle features. It becomes evident that the different possibilities of layout are characterized by the arrangement of the main circuit components inside or outside the prestressed concrete pressure vessel, by their interconnections and by the design of the turboset. These essential layouts are categorized as non-integrated (N), fully-integrated (J) and partly-integrated (T) systems and their different types of layout are compared in the different columns of Fig. 2.

Going from left to right the possibilities of location of components are compared, starting from installation of the heat exchangers in an annular space around the core and ending with the pod type layout.

Based on the analysis of the possible plant layouts, those offering good prospects were investigated regarding their technical feasibility, capability of safety requirements, availability, and economics.

In the non-integrated system, only the reactor is located in the prestressed concrete pressure vessel; the pressure vessel and the other components of the circuit are connected to the reactor via ducts (cf. Fig. 3).

The circuit components arranged outside of the prestressed concrete pressure vessel: compressors and turboset, heat exchanger, and coolers are located inside of a containment together with the prestressed concrete pressure vessel.

The gas, coming from 4 vertically arranged recuperative heat exchangers, enters the upper part of the reactor by 4 radial openings in the prestressed concrete pressure vessel, and exits to the turbine by a central pipe in the bottom of the prestressed concrete pressure vessel. The gas exit from the reactor contains a shielding body to protect the turbine against excessive radiation. This body simultaneously serves as a cross section limitation.

All high pressure gas lines ending in the reactor are manufactured as double wall pipes. For safety considerations even the inner pipe is designed for the full pressure difference against ambient pressure.

Two of the four decay heat coolers located above the prestressed concrete pressure vessel, serve as reserve units. The decay heat coolers are connected to the reactor by a double wall pipe and are located in a safety container.

All gas lines to and from the turboset are connected to the upper part of the casing. Apart from the turbine inlet, for all gas lines two parallel lines are provided. For assembly, inspection and repair purposes the lower part of the casing of the turboset with the rotor can be lowered by a special hydraulic or mechanic device; the upper part of the turbo-set casing and the generator remain on the foundation.

In this layout four precoolers and four intercoolers are provided. The precoolers are directly connected to the recuperative heat exchangers. All heat exchangers operate in counterflow and are manufactured as pipe heat exchangers.

In the partly integrated system, the reactor is located in the prestressed concrete pressure vessel, the turbine is installed in a machine cavity, and the heat exchangers and

precoolers are located in pods (Fig. 4). A second prestressed concrete pressure vessel contains the compressors, inter-coolers, and the helium purification systems. This vessel is designed for a pressure of 8 bar which represents the equilibrium pressure after the maximum loss of coolant accident. Both pressure vessels are connected by a common foundation platform.

This layout shows essential characteristics of the integrated system.

The helium flow from the reactor enters the turbine via a shutter tube in the high pressure prestressed concrete vessel. (The turbine is flanged directly to the shutter tube and is protected by shielding structures against excessive radiation.) From the turbine the helium flows through the free part of the machine cavity into the heat exchangers and into the precoolers installed above. Then the cooled gas flows downwards through the channels in the concrete and in horizontal direction through the foundation plate to the compressors with an intermediate cooling stage. The high pressure gas is also directed through the foundation plate to the heat exchangers. It flows along the tube bundles of the heat exchangers in cross counter flow and enters the reactor laterally at the height of the lower reflector, cooling the metal structures before being reheated in the core. Thus, all handling and measuring equipment for fuel elements are located in the region of low gas temperatures. Otherwise the reactor corresponds to those used with steam turbine plants.

The main circuit is integrated up to the precooler outlet. This ensures sufficiently low pressure change rates in the integrated portion of the main circuit, in the event of rupture of a gas pipe, because of the throttling effect of the turbine, the large gas volumes of the heat exchangers

and precoolers, and the parallel pipes leaving the prestressed concrete pressure vessel. The O_2 content of the gas resulting from the mixture of air and helium does not constitute a danger to the reactor.

Removal of decay heat is effected by a redundant system consisting of a precooler, a blower, and a heat exchanger, located in two additional cavities in the prestressed concrete pressure vessel.

In the integrated system all components are located inside the prestressed concrete pressure vessel (Fig. 5). Penetrations through the pressure vessel are required only for the shaft connecting the turbine with the alternator, and for assembly and disassembly of the components located within the vessel.

A two-room prestressed concrete pressure vessel is envisaged (without pressure separation). Heat exchangers and coolers are arranged in the annular space around the reactor and the compressor turbine unit, respectively. Compressors and turbine are located in a separate room below the reactor and are protected by shielding structures. In this room also the coolers are located which operate in a pure helium environment, as the compressor turbine unit.

The coolant gas flows from the core through the shielding and through a short line, containing a shut-off valve, into the turbine. From the turbine it flows upwards to the heat exchangers and downwards to the coolers and compressors.

This design is characterized by the features that all components are interconnected by ducts, no duct of the main circuit, however, leaves the prestressed concrete vessel, and that the room containing the compressor turbine unit

and the coolers must be kept at the maximum helium pressure and, using inventory control, the pressure level within this room will vary with the pressure level of the whole system.

For emergency cooling two emergency cooling blowers with variable speed and emergency coolers are provided.

This system is also characterized by a two-room prestressed concrete pressure vessel without pressure separation, the heat transfer units are, located in pods (Fig. 6). The other characteristics correspond to those of the layout shown in Fig. 5.

In the integrated system in Fig. 7 the compressor turbine unit is located in a machine cavity. The intercoolers are arranged in horizontal openings in the lower part of the prestressed concrete pressure vessel. In addition, this layout is characterized by the feature that all gas ducts are formed by channels in the concrete of the prestressed concrete pressure vessel and in the slab separating core and machine cavity.

CONCEPT PROBLEMS AND TIME SCHEDULE OF DEVELOPMENT WORK

The various layout concepts described show a great number of common and specific problems. The investigation of these problems is being carried out within a large systems analysis. To decide which design concept will be further developed an evaluation of the individual concepts is being carried out within 1970. Applying common safety standards the evaluation is carried out according to the following criteria:

- feasibility
- availability and
- costs.

The main items which are being investigated, are:

- assembly, disassembly, pipe connections, accessibility, foundation layout of the turboset
- layout of the gas pipes of the main circuit
- flow behaviour after pipe ruptures and their effects
- design and location of shut-off valves of the main circuit
- handling helium volume during control procedures and repair
- openings and penetrations of the prestressed concrete pressure vessel.

The concepts described are the result of the first design phase (systems analysis). The design will be developed in sufficient detail so that in 1970 a decision can be made on the general layout. The decision regarding the type of fuel elements is expected in 1972. Complete construction documents will be available in 1975.

Components will be tested in three helium test loops which are under construction. By extensive fuel element development work, including an investigation of the deposition mechanisms of fission products, account will be taken of the special conditions of the single-cycle plant. The experience gained with the operating high temperature test reactors and the prototype power plants under construction or to be constructed in the near future, will considerably contribute to the successful execution of the project.

	A	B
max. processtemperature	850 °C	850 °C
min. processtemperature	22 °C	35 °C
number of intercoolers	2	1
max. processpressure	60 bar	60 bar
thermal reactor power	1333 MW	1428 MW
electrical output	600 MW	600 MW
net efficiency	45%	42 %

Fig. 1. Nominal Design Data

Fig. 2. Various layout schematics

Fig. 3. Non-integrated layout

Fig. 4. Partly integrated layout

DISCUSSION

S. I. Kaplan: How would you provide emergency cooling?

H. Oehme: All designs described have two auxiliary loops for emergency cooling which can operate at all pressures from full pressure to atmospheric.

J. D. Thorn: To what extent is your selection of plant layout influenced by a preference for a horizontal shaft and a single machine?

H. Oehme: In the beginning of our analysis our turbine designers looked for the most desirable solutions for their point of view. They proposed the single shaft horizontal machine, so we made it a basis of our layout analysis. The reasons for that are described in the paper. I may mention one point - having a single shaft horizontal machine or vertically arranged compressor turbine units with a horizontal power turbine as in some British proposals, one needs in both cases approximately the same lower part of diameters in the lower part of the PCRV for placing or replacing the power turbine.

L. J. Connery: Will your helium turbine discharge its waste heat to air rather than a cooling water source?

H. Oehme: All the layouts discussed in the paper incorporate an intermediate water cooling circuit for safety reasons and a final disposition of heat to the air to acquire site independence. Because of the relatively high off-gas temperature in the HHT this solution is technically feasible and burdened with a reasonable decrease in efficiency only.

G. Meijer: Regarding the problem of fast closing isolation valves in connection with nonintegrated and partially integrated layout, I would like to make the following remarks: In spite of decay heat removal loops being capable to remove the decay heat after a fast depressurization, I am afraid that one still will need fast operating isolation valves for nonintegrated designs. Based on the present knowledge, I must emphasize this, the reasons are: (1) Difficult core levation problems possibly even with a venturi in the ducts, (2) high temperatures of the air-helium mixture in the secondary containment during and after depressurization

through a main coolant duct. If one needs fast closing valves, they will have to operate in a high temperature environment for the nonintegrated designs. Although fast closing valves may be technically feasible, a long development period and considerable cost will be involved and even if technically feasible a licensing problem must be anticipated. In a partially integrated design, one may or may not need fast closing isolation valves. This has not been determined yet. However, if we would need them in this type of layout, the valves would be located in the cold ducts. They, therefore, are more feasible from a technical standpoint and easier to license. Because of the above reasons, I am of the opinion that nonintegrated designs do not seem to be as feasible and that one should concentrate on partially integrated and fully integrated designs. In doing this the requirements for nonintegrated designs will automatically emerge.

H. Oehme: I fully agree. In fact, I may add two remarks with respect to the nonintegrated designs. We have already had the experience, that even if the feasibility of an emergency closing valve is a problem it may not be accepted by the licensing committee, even if you have two valves in series. For the reasons mentioned by you and for other reasons, we, in fact, tend to favor one of the integrated designs.

L. A. Lys: You have additional emergency cooling loops in your design. Does it mean that you must rely upon a fast closing valve to isolate the main loop in order to avoid bypassing of the core in the case of emergencies?

W. Twardziok: We don't need quick closing valves to operate the emergency cooling system, because we have time enough before starting the emergency cooling. We agree that we have to isolate the main loop to decrease the mass flow for the emergency cooling, but that can be done slowly.

L. A. Lys: In your integrated design I have not noticed space for helium which must be removed from the loop in the case when the pressure level control system is used. If the helium is stored outside of the pressure vessel pipes, penetrating the wall of the vessel is, of course, necessary. This, however, will conflict with the principles of the integrated design.

W. Twardziok: For all HTR gas turbine designs (integrated or non-integrated) we proposed to have a combination of bypass and pressure level control. Quick changes will be done with the bypass control. For the pressure level control there will be more time, that means only small diameter pipes for the connection of the primary circuit and the helium storage system, arranged outside of the pressure vessel. Loss of coolant must be taken into account also for the integrated systems, but it means only small depressurization rates which leaves enough time for valve action.

DEVELOPMENT WORK FOR LARGE-SCALE NUCLEAR HELIUM
TURBINE PLANTS WITH HIGH-TEMPERATURE REACTORS

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ABSTRACT

The main part of the development programme for high-temperature reactors with helium turbine is proposed to be spent on test facilities and test work. Three test rigs for the non-nuclear tests on helium components and systems are described. The rigs are proposed to be erected jointly with the auxiliary installations in a common test field on the premises of KFA in Jülich. The problems of test work are briefly described by the two examples, hot gas valves and insulation.

1. Introduction

Within the framework of the development programme for high-temperature reactors with helium turbine of high output (HHT-programme), an extensive test programme will be performed parallel to the projecting work. The importance of this part of the programme can be seen from a cost comparison, which shows that about 80 % of the total cost is proposed to be spent on test work and 10 % each on the projecting work and on an accompanying programme for basic research.

The nuclear test programme requires about 25 % of the total cost, it comprises mainly the development and testing of fuel elements and materials. For the non-nuclear test programme about 55 % of the total cost is envisaged; here the task is to develop and test reactor and helium turbine components and systems. The overall idea is to prove, with the aid of the test programme, the technical feasibility, the safety and the economy of components and systems for large HTR-plants with a direct cycle.

The following is dealing only with the non-nuclear test programme, which can be divided into two phases. The first phase comprises non-concept-bound tests on smaller test rigs and the erection of rigs for testing full-scale components of the 600 MWe-plant. The second phase covers the concept-bound tests and the testing of prototypes.

2. Test rigs

For carrying out the test and development work, existing helium test rigs will be concentrated in one place and completed by loops in which the parameters of the coolant are to be realized which are envisaged

for the nuclear power plant being projected. In all installations helium is circulated at high temperature and pressure, with reactor impurity levels being aimed at.

The arrangement and main data of the three helium test rigs are shown in Fig. 1. The helium test loop (HVK) consists of the circulator (a), the heat exchanger (b), the test section (b'), the heater (c), the mixer (d), the cooler (e) and the motor (f). At a mass flow of 1 kg/sec in the test section a temperature of 800°C and a pressure of 20 bar is achieved. The main components of the helium test turbine rig (HVT) are the LP compressor (a), the intercooler (b), the HP compressor (c), the heat exchanger (d), the natural-gas-fired heater (e), the turbine (f), the pre-cooler (g), the by-pass cooler (h), the test section (i), the gearbox (k) and the starting motor (l). The mass-flow in the test section is about 6 kg/sec at a temperature of approximately 700°C and a pressure of 22 bar. The high-temperature helium test plant (HHV), with a mass flow of about 220 kg/sec at a maximum temperature of 1000°C and a maximum pressure of 50 bar, is proposed to be used for prototype testing. Its main components are the hot-gas compressor (a), the test section (b), the turbine (c), the mixer (d), the by-pass cooler (e), the gas circulator (f) and the driving motor for the turbo set. The thermal energy for the loop is supplied by the motor driver of the turbo set.

The helium test loop (HVK) is proposed to go into operation in the autumn of 1970. A view of the HVK model is shown in Fig. 2. In the foreground is the vertical test section, on the right is the heater

and in the background is the vertically arranged heat exchanger.

The helium turbo set of the helium test turbine rig is shown in Fig.3 during assembling. From left to right one can see the LP compressor, the HP compressor and the helium turbine with axial outlet, still equipped with nozzle rings. This first helium turbine set of the world was originally used under the name of La Fleur in Phoenix, Arizona (USA), in a nitrogen liquefying process. The machine set was built by Escher Wyss, Zurich. It is under modification by GHH to become a testing installation. Fig. 4, which shows the helium heater of about 15 m height, gives an impression of the size of this test rig. The heater is fired with natural gas; the thermal output is about 7 MWth and the gas consumption about 1300 Nm³/h. Between the heater and the turbine there is an austenitic pipeline onto which the test section is to be connected.

A cross section of the turbo set of the BBC-planned high-temperature helium test plant is shown in Fig. 5, with 1 being the machine casing, 2 the hot-gas inlet, 3 the hot-gas outlet, 4 the by-pass inlet, 5 the by-pass outlet, 6 the turbine blading, 7 the rotor, 8 the compressor blading and 9 the oil-lubricated radial bearing. The two-stage turbine has an output of about 50 MW, while the 10-stage compressor has a power requirement of about 90 MW. The additional power required is supplied by a synchronous motor with starting equipment. For cooling the ferritic-disc rotor and for sealing there is a separate blower circuit. The rotor of the turbo set has about the same dimensions as that of a 300 MWe-helium turbine set.

The test rigs dealt with in the foregoing are proposed

to be erected jointly with the auxiliary installations in a common test field on the premises of KFA, the nuclear research centre in Jülich. The ground plan on the helium test field at present in the planning stage is shown in Fig. 6. The smaller installations, the helium test loop (HVK) and the helium test turbine rig (HVT) are accommodated in the hall of about 12 m height on the right and the high-temperature helium test plant (HHV) in the hall of about 20 m height on the left. In a lower connecting bay are the common helium supply system with the storage tanks and the common helium purification plant. Above these the control stations for the rigs and the data collection system are arranged. The test halls are preceded by the shop, laboratory and office building. On the left and right beside the halls are the power feeding and distributing stations so that there is only a short way to the consumers. The erection of this building is scheduled to be completed by the middle of 1971 to the extent that operations can be started. The test operations are proposed to be carried out by the group of BBC, GHH and FK in cooperation with KFA.

Apart from the afore-described installations, a conventionally fired 50 MWe-helium turbine power station is proposed to be erected under the HHT programme, in order that all components of a helium turbine installation can be combined and tested in continuous operation in a real power station circuit. The main data of the proposed plant are shown in Fig. 7. The turbine inlet temperature of 730°C applies to continuous operation. For testing purposes it is possible to achieve higher temperatures for a short time. With a maximum pressure of 40 bar, an expansion ratio of 2.5, compressor inlet temperatures of 25°C , single-

stage intermediate cooling and a temperature approach in the heat exchanger of 30°C , a mass flow of 86 kg/sec is achieved. The net output is 50 MWe and results, together with the thermal output of the heater of 156 MWth, at a net efficiency of about 32 %. In the 50 MWe-plant it is intended to test systems and components of large dimensions for reliability and safety in continuous operation and to ascertain the dynamic behaviour and the joint operation of the components of the helium turbine plant.

3. Test programme

During the first project phase, in which the design concept of the 600 MWe-plant is to be found, only such tests can be carried out which are of a basic, i. e. non-concept-bound character. For these tests, which serve to support the judgement and choice of the first plant concept, the two small test rigs HVK and HVT are suitable. For cost reasons the components of original dimensions can only be built and tested after completion of the reference design of the 600 MWe-plant. They are intended to serve as prototypes for direct preparation of final drawings and data for the 600 MWe-plant.

3.1 Survey

The time schedule for the most important tests is shown in Fig. 8. At the top the three test rigs are given, starting from commencement of the test operations. The gas ducts with the hot-gas insulations and expansion bellows are first to be examined by preliminary tests on the HVK, thereafter, with more complicated geometries, on the HVT

and finally, with the dimensions resulting from the reference design, on the HHV rig. The same procedure will be adopted for the hot-gas valves, which, depending on the concept, may be equipped with or without trip gear. The flow distribution, thermal-shock behaviour, tightness and dynamic behaviour of the heat-exchanging units will also be investigated step by step on the three test rigs. The hot testing of the reactor internals is proposed to be carried out on the HVT and HHV rigs. While the tests aiming at design solutions to safety problems are carried out on all of the three rigs, the dynamic behaviour of a helium turbo set and the other components can only be studied on the HVT. However, the main results will have to come from the 50 MWe-plant.

Finally the shaft seals are shown, which will be developed at a separate test installation and tested during operation of the plants. The HHV rig is proposed to be equipped with a shaft seal whose dimensions correspond to those of a 600 MWe-turbine seal.

The specifications for the various tests are under preparation. The problems are briefly described in the following by two examples.

3.2 Hot-gas valves

HTR plants with helium turbine require shut-off and control valves of large diameters for operation in helium of high purity at high temperature and pressure. Especially in plants which are not fully integrated, these valves must have a high inner and outer tightness and a short closing time. Fig. 9 shows a design of a non-conventional hot-gas valve

with small dimensions. Such a valve with small dimensions is proposed to be examined by preliminary tests before a large unit is tested.

The pneumatic control principle is briefly described in the following (see Fig. 9). On normal opening and closing operations the valves V5 and V6 are actuated. On trip-closing the servo valve 1 is operated, with a sufficient quantity of gas being caused, by actuation of control valve V1, to flow into the lower cylinder space from the pressure accumulator 5 through the charging line 6. Before reaching its upper end position, the valve plate is braked by the damping pin 4. The blocking pin 7 fixes the plate in its upper end position; it is operated via the valves V3 and V4. During every valve motion the load on the two sealing plates 8 is removed to the LP tank through the pipes 2. The control system is fed from the auxiliary supply system 9. The entire valve casing and the valve plate have to be kept at a lower temperature by means of the cooling gas 3.

The most essential difference as compared to conventional designs is that the valve plate, which is guided in needle bearings, has also the function of the control slide. This design avoids penetrations and is particularly compact, which is advantageous especially for installation in the prestressed-concrete vessel. The most important problems to be examined are :

- cold welding of parts in motion and at rest,
- material abrasion,
- wear of needle-bearing guide,
- tightness at seats,
- control-gas requirement and closing forces,

cooling-gas supply and requirement, and efficiency of damping.

The purpose of the investigations is to prove the functional reliability and safe availability with respect to application of such valves in the safety concept of large HTR-plants with helium turbine.

3.3 Hot-gas insulations

The special problems of hot-gas insulation in HTR-plants with helium turbine result from

the system-inherent high-pressure-changing rates during control operations and accidents,

the high gas flow velocities in the gas ducts due to the large volumes and the correspondingly high-pressure drops and dynamic forces,

and the high gas temperature and pressure with helium of reactor impurity.

Fig. 10 shows the most important insulation geometries to be examined. These are the straight insulation section (a), the bend (b), the T-branching (c) and the straight vessel wall (d). In addition to these geometries there is the insulation at the turbine inlet, which may have particularly complicated geometries.

With regard to the nuclear purity conditions, the insulations used in reactor building are either all-metallic foil or mat insulations or ceramic fiber insulations with tight metallic liners.

Fig. 10 shows as an example the connection of two sections of a foil insulation (e) and a pipe branching (f) having a fibrous insulation with a

liner. In designing these insulations it is necessary to provide the clearances or openings required for thermal expansion and depressurization in such a way as to ensure that the dynamic behaviour is not badly affected and hot spots are avoided.

The problems to be investigated can be divided into three main groups :

- (a) the thermodynamic behaviour with regard to heat conductivity,
- (b) the mechanical behaviour with regard to thermal expansion and depressurization, and
- (c) the vibration behaviour with regard to alternating stress from gas and sound forces and mechanical vibrations.

The testing is proposed to be performed step by step. At the beginning there will be preliminary tests on the helium test loop concerning the thermodynamic behaviour of a pipe insulation in helium at high temperature, pressure and flow velocities.

For these tests a special test section has been designed and built, which is shown in Fig. 11. The inner pipe (a) is provided with a metal foil insulation (b) which carries the displacement body (c). At the displacement body the static pressure tapplings (d) are fixed. A joint of the insulation sections (e) is shown in the lower part and the bellows to absorb thermal expansion in the upper part (f). The hot gas enters at 800°C at point 1 and leaves at point 2, the cooling gas at 350°C flows in counter-current from 3 to 4. The test section is being provided with the instrumentation and

the tests are proposed to start at the end of this year.

With the displacement body it is possible to achieve gas flow velocities of up to about 100 m/sec, so that pressure drops per insulation section can be achieved and even exceeded as will occur in the future large unit. Fig. 12 shows a comparison of the test section data of the three loops with the data of the 600 MWe-plant. As can be seen, it is necessary to provide the insulation test sections of both the HVK and HVT with a displacement body to achieve the gas flow velocities required for ascertaining the thermodynamic behaviour. The integral behaviour of the insulation can only be examined with the original dimensions of the pipe on the HHV rig as in this installation comparable dynamic gas forces are expected.

Parallel to the thermodynamic tests described, the insulations, especially with helium turbine plants, have to be tested with regard to their behaviour on rapid pressure changes. Other than in HTR two-cycle plants, it is necessary even in normal control operation, to master pressure changing rates which amount to about 10 bar/sec and are thus higher by about the factor 10 to 20. Depending on the arrangement of the plant as a whole (integrated or non-integrated), rates of up to 100 bar/sec may occur in an accident such as pipe rupture.

A depressurization vessel in which these high changing rates are achieved is shown in Fig. 13. In the tank (a) a pipe section with a test insulation (b) is fitted. At the outlet nozzle in the upper head it is possible to set different cross sections

by means of an orifice, which before commencement of a test is covered by two bursting discs. These discs are caused to burst during the test by admitting pressure gas to the space between them. The pressure differentials between the pipe interior and the interior of the insulation or behind the insulation are measured during depressurization as well as the deformation of the inner liner at the joints.

The first preliminary tests on a foil insulation with cold gas have been started at EURATOM in Ispra. At present a larger test vessel is designed by GHH, in which insulation geometries of original dimensions can be tested also at high temperatures. The flow tests and depressurization tests will be carried out on insulations with the same design and geometry.

Fig. 1 Arrangement and Main Data of the Helium Test Rigs

HVK (helium test loop) HVT (helium test turbine)

a circulator
 b heat exchanger
 b' test section
 c heater
 d mixer
 e cooler
 f motor

HHV (high-pressure helium test rig)

a compressor
 b test section
 c turbine
 d mixer
 e by-pass cooler
 f circulator
 g driving motor

a LP compressor
 b intercooler
 c HP compressor
 d heat exchanger
 e heater
 f turbine
 g pre-cooler
 h by-pass cooler
 i test section
 k gearbox
 l starting motor

Fig. 2 Model of a Helium Test Loop (HVK)

Fig. 3 Turbo Set of the Helium Test Turbine (HVT)

Fig. 4 Gas-Fired Heater of the Helium Test Turbine (HVT)

Fig. 5 Turbo Set of the High-Temperature Helium Test Plant (HHV)

1 machine casing	6 turbine blading
2 hot-gas inlet	7 rotor
3 hot-gas outlet	8 compressor blading
4 by-pass inlet	9 oil-lubricated radial bearings
5 by-pass outlet	

Fig. 6 Helium Test Field for HHT Development
in Jülich

Turbine inlet temperature	$^{\circ}\text{C}$	730
Turbine inlet pressure	bar	40
Pressure ratio	-	2,5
Compressor inlet temperature	$^{\circ}\text{C}$	25
Number of intercooling	-	1
Heat exchanger temperature difference	deg C	30
Mass flow	kg/s	86
Net power	MWe	50
Heater thermal power	MW _{th}	156
Net efficiency	%	32

Fig. 7 Preliminary Layout Data of the Conventionally
Fired 50 MWe-Helium Turbine Power Station

Fig. 8 HHT Test Programme

Fig. 9 Hot-Gas Valve for Helium Turbine Plants

1 servo valve	6 charging line
2 to LP tank	7 blocking pin
3 cooling gas	8 sealing plate
4 damping pin	9 auxiliary supply system
5 pressure accumulator	V1 to V6 control valves

Fig. 10 Problems with Hot-Gas Insulations

a	straight insulation section
b	180° bend
c	pipe branching
d	vessel wall
e	foil insulation
f	fibrous insulation at a pipe branching

Fig. 11 Test Section for Hot-Gas Insulations

a	inner pipe	f	bellows
b	foil insulation	1	hot-gas inlet
c	displacement body	2	hot-gas outlet
d	static pressure tappings	3	cooling-gas inlet
e	joint of insulation packs	4	cooling-gas outlet

	Pressure bar	Temperature °C	Mass- Flow kg/s	Diameter of Hot Gas Duct mm	Velocity of Gas m/s
Helium Test Loop HVK	20	800	1	216 ¹⁾	100
Helium Turbine Test Loop HVT	22	700	6	400 ¹⁾	76
Helium High Temperature Test Plant HHV	50	1000	220	1020	142
600 MWe-HHT-Plant	60	850	700	1570	140

¹⁾ with displacement body

Fig. 12 Main Data of the Helium Test Rigs

Fig. 13 Depressurization Vessel

a vessel

b test insulation

DISCUSSION

B. N. Furber: I would be interested in further details of the metallic insulation proposed for the duct insulation, particularly for the 180° bend. Here the difficulties of insulation combined with a very complex static pressure field make the insulation problems (particularly leaking) very severe.

W. Twardziok: I fully agree that there are special problems concerning the duct insulation for bends. Therefore, we propose to do tests on these geometries with foil insulation as well as with fiber insulation. In both cases we have to take care for the sealing with respect to the forced convection on the one side and for the openings with respect to the depressurization rate on the other side. For our first bend test section we propose to take a metallic foil insulation which is divided into several sections and weld it to the pipe circumference on one end.

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THE PROGRAM OF PLATE-OUT INVESTIGATIONS
IN THE GAS TURBINE PROJECT

C.B. von der Decken

4/533000

The HHT-project, the project for the development of a HTR-system with direct cycle gas turbine has been described extensively in the foregoing papers. In this paper I want to speak about a special subject from this project, the plate-out of fission products.

Due to the development of the coated particles the fission products are retained very well inside the fuel elements. One need not speak at present of a contaminated cooling gas system in HTR's. The level of activity in the primary system of HTR's is comparable to the level in the primary system of reactors with metallic clad fuel elements, at least in practical reactor operation taking into account problems of the production failures, etc. This has been demonstrated by a large number of in-pile experiments, but especially by the operation experience in three reactors, -Dragon, Peach Bottom and AVR.

Nevertheless, the low-level contamination of the cooling gas and the components is still large enough to play an important role in the safety philosophy and possible accidents. The lack of detailed knowledge of the behavior of fission products leads to a pessimistic view in the safety philosophy and with this to an over-emphasis of the safety provisions in the layout of the plant which could be an important factor in the economics of the HTR's.

This is a problem for all HTR-systems but specially for a system with a direct cycle gas turbine because of the relatively large conduit cross-section which could be opened by a practical or hypothetical accident. In such a case the depressurisation of the plant would be much quicker and higher gas velocities would occur in many parts of the system. In a large loss of coolant accident therefore the release of fission products out of the primary system could be much higher compared to steam cycle systems. Furthermore the distribution of fission products over the surface of the system is important for maintenance and repair because the gas turbine itself represents a large piece of equipment and the necessary precautions of shielding and safety for the maintenance operations will have an important influence on the design, the costs and the availability. For these reasons in the frame work of the HHT-project an extensive program has been developed to study the problems of the plate-out of fission products. I will try to give an overview of this program.

The discussion of such a program should start with a summary of what is known up to now. Over the previous years important and good work has been done specially at Batelle, Oak Ridge and Gulf General Atomic. But I believe I can avoid giving such a summary today because this will be the subject of a special paper later in this conference.

The plate-out program in the HHT-project consists of the following main items.

- 1) Calculation models and computer programs
- 2) Measurement of the adsorption and desorption rates
- 3) In-pile experiment "Saphir"
- 4) Out-of-pile experiment "Smoc" (Saphir model out-of-pile
circuit)
- 5) Adsorption of fission products on graphite dust
- 6) Investigations in the AVR-reactor

Due to limited time I will be able only to discuss the main ideas of the different items and to give a short description of the experiment "Saphir".

CALCULATION MODELS AND COMPUTER PROGRAMS

To understand the behavior of fission products in complex system like primary circuits in HTR's a number of calculation models have to be worked out. The ultimate hope is that it might be possible to describe the complete process in the cooling system with a large computer program.

The mass transport in the gaseous phase can be described by the known laws of mass transport and heat transfer under flow conditions. Immediately near the wall there exists a boundary layer out of which the adsorption or desorption takes place. To work out a calculation model one can assume as a first step that the adsorption equilibrium with a boundary layer takes place spontaneously. With this assumption we are now preparing a computer program "fish-trade No. 1" (fission product transport and deposition) which describes the temporal and local behavior of the fission products in any given complex tube system. Tube sections in parallel and in series, different temperatures, tube profiles, Reynolds numbers and adsorption constants can be taken into account. The program is based on the description of the adsorption by the laws of Henry and Langmuir. It will be ready soon.

The second step will be the program "fish-trade X" which corresponds to the program "fish-trade No. 1", but experimentally determined adsorption isotopes will be used which will have been measured for a number of fission products and materials. The next step will be the program "fish-trade Y" which corresponds to the Predep-program from Kress not using the assumption of spontaneous equilibrium in the boundary layer. In this program also, experimentally determined adsorption and desorption rates will be used. One might better use in this case the expression, reaction rates, because in addition to the adsorption effects, chemical reactions of the fission products with structural materials will play an important role.

MEASUREMENT OF THE ADSORPTION AND DESORPTION RATES

To use the computer programs a large number of characteristic basic data have to be measured; for example, the adsorption and desorption rates for the most important fission products on the structural reactor materials. These basic data depend on a large number of parameters.

The fission products of main interest are Sr, I and Cs. Ba, Ce, Ru and Te are of minor importance. The adsorption materials are all materials which are in contact with the primary cooling gas. This means ferritic and austenitic steels with different surface properties and graphites with

different structure and surface areas and other ceramic materials.

For the fission products Sr, I and Cs the already known measurements of the adsorption isotherms under static or laminar conditions will be repeated and extended. In the literature a large number of such measurements have been described. Specially the publications of Zumwalt and co-workers and those from ORNL have to be mentioned. These measurements are already giving a rough picture of the circumstances, but many of the detailed results are still contradictory and they cannot be extrapolated to other materials and conditions. During these experiments in our program the following view points will be taken into account particularly.

- a) We will select only materials as an adsorber which will be important for the technical lay-out of a plant. The surface properties and conditions of these materials will be defined as clearly as possible. The surface conditions of some of the materials will change during reactor operation and we will specially look in this problem. The influence of oxidic layers on steel surfaces is an example.
- b) In the work already published the adsorption of the different isotopes has been measured for single isotopes only. The influence of any co-adsorption has been neglected. Probably the influence of the co-adsorption is only important near the saturation. Adsorption isotherms had to be measured up to saturation and repeated in the presence of other fission products, for example: Cs/Sr.
- c) Furthermore, an important parameter will be the level of impurities in the cooling gas. This influences the condition of the adsorption surfaces and the fission products themselves. Concentrations of only a few μ atm of water vapor or carbon-dioxide in the cooling gas represents some orders of magnitude more atoms per cm^3 compared with the concentrations of fission products. This means that, for example, Cs, which might be created in the atomic form would have a good chance to react with the impurities of the cooling gas before reaction with any surface takes place.

Furthermore, the measurements of the adsorption isotherms will be extended to turbulent flow conditions. Up to the present such measurements have been done only up to 20 m/sec max. gas velocity.

Since there will be much higher gas velocities in the reactor system we want to measure up to 100 m/sec gas velocity in maximum. This is because it has not been clearly established today whether the velocity of turbulent motion immediately next to the wall is comparable to the mean velocity of the molecules. It might be sufficient to make experiments for a few typical cases only.

A next group of important data are the desorption rates especially for safety predictions in case of reactor accident with loss of coolant. The measurements of desorption rates have to be done under static or laminar and under turbulent flow conditions.

In addition to adsorption, chemical reaction of the fission products with the structure materials will be important. In some cases the mass transport coefficient must be considered, while in other cases the velocity constant of the surface reactions will be the more essential. The problem has not been examined systematically up to now. As the mass transport coefficient for the most common flow conditions and geometries is well known we will measure in the next series of experiments the velocity constants for a number of important reactions and the time dependence. If the methods prove to work well, we will also study the temperature dependence. Based on the results of these experiments it should be possible to determine whether the quantity of adsorbed fission products for various materials and conditions depends mainly upon chemical kinetics of surface reactions or upon the convective mass transfer coefficient.

The experiments to measure the adsorption and desorption rates under static or laminar conditions will be done in an apparatus which is shown schematically in Fig. 1. A quartz tube contains the adsorption material and the source fission product. Independent heating provides for temperature regulation and for establishing given partial pressure. For desorption measurements the system can be evacuated.

Part of the experiments with iodine will be done with inactive iodine. We hope to develop the detection technique for iodine to a high level of sensitivity. The advantage of using an inactive apparatus is, of course, a simpler technique, and greater flexibility. We hope also for better accuracy to define the different parameters such as partial pressure, etc. A schematic drawing of the apparatus is shown in Fig. 2. The experiment will be done in standard atmospheric conditions. Clean air will be mixed with iodine vapor. The partial pressure of the iodine can be regulated by the temperature of the iodine source. The apparatus will be started by by-passing the test section until the partial pressure is constant. Then the gas

stream will be switched over to the test section. By this method conditions during the start-up of the experiment will be avoided. The same apparatus can be used for desorption measurements.

The experiment Smoc which will be described later will be used for all the other experiments under turbulent flow conditions.

IN-PILE EXPERIMENT "SAPHIR"

In the reactor Pégase in Cadarache we are preparing an irradiation experiment in the Loop Saphir, which we consider as an integral test with respect to the several out-of-pile experiments mentioned above. This experiment will last about two years. We will measure the deposition of solid fission products released from spherical elements under operation conditions of a gas cooled high temperature reactor. We will find out the dependence on Reynolds number, temperature, different materials and surface conditions of the materials. The gaseous impurities of the helium coolant will be monitored and controlled continuously.

Description of the Loop "Saphir"

The loop consists of the main circuit with the test assembly in an in-pile position of the reactor and an auxiliary circuit for purification and analysis of the coolant. The auxiliary circuit is connected to a bypass of the main circuit in an out-of-pile position.

The main circuit

Fig. 3 shows the scheme of the main circuit. The helium coolant flows from the blower through an electrical pre-heater (110 kW) and a Venturi nozzle measuring the total helium-flow to the test assembly. Here the helium divides into two streams. About 90 % of the helium flows through an outer annulus of the test assembly back to the blower. Depending on the test conditions one can lead a part of the helium through a heat exchanger, which is placed in a bypass in front of the blower. The other part of the helium flows through the test assembly to the auxiliary circuit and then back to the main circuit.

The main circuit is mounted in a frame which is lowered to the bottom of the reactor tank. It then is moved to the core on rails.

Fig. 4 shows the test assembly. In a graphite sleeve 14 sphere elements each with a diameter of 60 mm are arranged in line. The sleeve is surrounded with Silicagel-wool insulation. Between the insulation and the outer tube of the test assembly is the annulus for the external helium-flow.

Above the fuel elements the interchangeable test tube is mounted. It is 2 m in length with a diameter of 30 mm, followed by a tube with a length of about 1 m and an inner diameter of 20 mm. The last one fits through the upper plug of the main circuit. A 180° - bend connects this tube with an absolute filter for solid fission products, both mounted out of the pressure tube of the main circuit in the reactor tank.

The absolute filter has a volume of 2 ltr. It contains some layers of a special glass fibre and silver plated molecular sieve, type 13 X.

The inner mass flow of the helium (10 % of the total flow) flows inside of the insulation over the fuel elements. Depending on the test conditions it is heated up from 250 - 400 °C entrance temperature to 800 - 850 °C at the exit of the sphere arrangement. Then the helium passes the test tube. The helium stream which flows outside along the insulation and the test tube cools the hot helium in the inner side of the tube to 500 - 650 °C.

The inner helium flows through the bend and the absolute filter which is cooled by water of the reactor tank. It then flows through a control filter filled with charcoal and a mass flow meter to the auxiliary circuit.

A part of the released solid fission products deposits on the inner side of the test tube and the bend depending on temperature and flow conditions. The rest will be adsorbed quantitatively in the filter.

The main circuit is laid out for an operation pressure of 60 ata and a maximum temperature of 550 °C. In a preliminary test the mean neutron flux in the fuel has been determined to be $6 \cdot 10^{13} \text{ cm}^{-2} \text{ s}^{-1}$.

The auxiliary circuit

Fig. 5 shows a scheme of the auxiliary circuit. Its operation pressure is 60 at, the gas temperature is about 40 °C. The helium coming from the main circuit is driven through the auxiliary circuit by a compressor with a maximum output of 9 m³/h at 40 at. In a bypass the gas will be analysed; in a second one it will be purified. In addition it is possible

to inject defined amounts of impurities into the helium.

The gaseous impurities in the helium are measured with a hygrometer and a gas chromatograph. The H_2 will be determined with a Cu O - filter followed by a hygrometer.

To measure the gaseous fission products a part of the inner mass flow (about 50 mg/sec) will be bypassed before the absolute filter and led through a mass flow meter into a filter for short-lived or another filter for long-lived fission products. The short-lived isotopes are determined by their solid daughter products.

The total activity in the auxiliary circuit is controlled by a monitor.

The purification facility consists of three elements. A filter with BTS-catalysator oxidizes H_2 and CO. The operation temperature is 200 °C. Behind the filter the helium is cooled to 10 °C.

Then the helium passes two molecular sieve filters working at room temperature. In the first one H_2O is adsorbed in the second one CO_2 .

Then a part of the helium flows through a charcoal trap cooled by liquid air. Its volume is 2,5 ltr. In this filter N_2 , O_2 , Kr and Xe is adsorbed.

To control the effectivity of the purification system it is possible to connect a gas chromatograph or a hygrometer behind the filters.

The purification system is laid out in duplicate to avoid interruptions in operation due to defects or the regeneration of the filters.

Experimental procedure

The test tubes together with the bends and the absolute filter are changed five times a year and analysed quantitatively with respect to solid fission products. Of special interest are the isotopes I 131, Sr 89, Sr 90, Cs 137, Te 132 and Ba 140. The concentration profile of the deposited solid fission products is measured in both longitudinal and radial directions of the test tube.

In case the activation by neutrons of the test tube is negligible a γ scan is performed in longitudinal direction in the hot cells. Then the tube is cut into equal segments each of which is leached and analysed γ -spectrometrically.

As test material several kinds of steel and graphite tubes such as may be used in a HT-reactor with different diameters are provided. The gas inlet and outlet temperature and thereby the surface temperature of the test tube is varied by changing the inner mass flow. The power output of the fuel elements is kept constant. Between two changes the experimental conditions are not varied.

Table 1 shows the operation data which are attained in the loop.

Spherical fuel elements of the HTR type are to be used. To get a sufficient output of solid fission products the coated particles are fabricated with a thinner coating than usual. The thickness of the sealing and outer coating is about 60μ . The kernal consists of UO_2 and $Th O_2$ with a ratio of 1 : 5. The lay-out of the elements allows a constant power generation of 3 kW per sphere for one year. To compensate for the fuel consumption the loop is moved nearer to the core during the test time. The reference temperatures are those of the fuel elements and the inlet gas temperature of the test tube.

In case the released rate of solid fission products is too small to produce measurable effects at the test tube the fuel elements are exchanged by a second charge. This charge contains coated particles with a buffer coating of 60 - 70μ but no sealing and outer coating.

In a preliminary test which will probably start at the beginning of July this year the commissioning tests of the loops are to be carried out with normal THTR fuel spheres. If this test is satisfactory the first radiations will start at the beginning of August this year.

Table 1 Data of operation

<u>gas temperature [°C]</u>	
entrance fuel	250 - 400
outlet fuel	800 - 850
entrance test tube	800 - 850
outlet test tube	500 - 650
<u>gas pressure [at]</u>	
	40 - 60
<u>gas flow [g/s]</u>	
total flow main circuit	100 - 250
test tube	15 - 24
auxiliary circuit	15 - 24
purification system	1
mean Reynolds number fuel	$4 \cdot 10^3$
Reynolds number test tube	$(2 - 3,5) \cdot 10^4$
<u>fuel element</u>	
mean power / sphere	3 kW
number of spheres	14
maximum surface temperature	1100 °C
loading of U 235 / sphere	2,5 g
mean thermal neutron flux	$6 \cdot 10^{13} \text{ cm}^{-2} \text{ s}^{-1}$

OUT-OF-PILE EXPERIMENT "SMOC"

A large reactor experiment like "Saphir" is very costly in operation and not very flexible. Therefore the possibilities of varying the different parameters such as material, surface conditions, geometries and temperatures etc. are very limited. Each experiment with a given set of parameters would require two to four months for completion. For this reason we are planing an out-of-pile experiment "Smoc" (Saphir model out-of-pile circuit) in which we can vary all necessary parameters. An important consideration for the lay-out of Smoc is to simulate the operating conditions of Saphir as closely as possible in order to correlate both experiments. It is hoped that the results which can be achieved with Smoc can be translated easily to in-pile conditions.

In addition to this important comparison of the two experiments, Saphir and Smoc, the main purpose of the Smoc experiment is to measure the adsorption and desorption rates under turbulent flow conditions. Different steel and graphite test samples up to 50 mm in diameter and 4 m in length can be used and may be examined at temperatures up to 800 °C. Gas pressures up to 80 at, gas temperatures up to 800 °C, gas velocities up to 50 m/sec and Reynolds numbers up to 10^5 can be achieved. The gaseous impurities will be monitored and controlled. More complex test sections will also be used to test different computer programs.

The experiment Smoc is still under design and I am not able to give you more detailed information today.

ADSORPTION OF FISSION PRODUCTS ON GRAPHITE DUST

In HTR's with prismatic fuel elements only minor quantities of graphite dust are present in the primary system. The quantity of graphite dust in pebble-bed reactors is somewhat larger. We are trying to determine the total amount graphite dust present in the AVR reactor.

Estimates have shown that the quantity of graphite dust in the AVR reactor is much less than had been assumed for the lay-out of the reactor. Nevertheless, even small quantities of graphite dust could be important in case of a large accident with loss of coolant if a large amount of fission products would be adsorbed on the dust. In such a case the dust could be blown out immediately during loss of coolant without any desorption process. Besides the adsorption rates the quantity of fission products on the dust depends on the surface area and this depends on the particle size distribution of the dust.

Experiments at Brown Boverie / Krupp which had been done in connection with the problems of erosion have shown that the graphite dust at high gas velocities in the cooling system is broken into small pieces very quickly. The dust particles under operation conditions are very small and have a large specific surface area. A typical particle size distribution of graphite dust which had been blown around in a circuit with a velocity of 10 m/sec is shown in Fig. 6. Curve 1 represents a particle size spectrum of the graphite dust which had been inserted before the start of the experiment.

Curve 2 shows the particle size distribution after 30 minutes of operation of the circuit. If the graphite dust is blown around in the circuits for a longer time the particle size distribution does not change very much. As one can see from the diagram the particle size is about a few μ .

In the investigation program we will make experiments which should define the graphite dust and will show how to handle this very fine dust. Later on we will insert dust in the experiments Saphir and Smoc to measure the amount of fission products which will be adsorbed on this dust. In addition we will try to find out how the dust will plate-out on surfaces and will be removed by a gas stream from the surface.

INVESTIGATIONS IN THE AVR-REACTOR

In model experiments it is very difficult to simulate the conditions of particle reactor operation well enough to extrapolate the results of these model experiments with sufficient certainty to actual reactor conditions. This is because in case of plate-out of fission products a larger number of parameters are involved and the corresponding data for the reactor conditions are not known. Measurements of fission product distribution in reactors in operation now could in principle give important results. Unfortunately it turns out to be impossible to test the calculation models on reactor systems in operation at present because of the lack of knowledge of all necessary data. In addition the number of possible investigations on reactor systems under operation is very limited due to time and design reasons.

In the first place the main objection of the AVR operation, for example, is to the testing of the whole system, the fuel elements and the components. Major interference in the system to measure the plate-out fission products on surfaces of the system would interrupt the reactor operation too much. In addition the AVR reactor is designed as a power station and this means that the possibilities for making detailed experiments are very limited.

Besides the routine measurement of the rare gas isotopes, the moderator spheres which contain only graphite are being investigated to measure the fission products on the surface of these spheres. Furthermore the surface of the primary system which has to be dismantled for maintenance and repair will be investigated for plate-out of fission products. Up to now, fortunately for the reactor, but unfortunately for these experiments, only components out of the secondary circuits had to be dismantled, for example, the components of the fuel handling system. In principle only the blowers and the butterfly valves for the bypass can be dismantled from the primary system. These components worked without failures up to now and dismantling is for inspection purposes later on. Also the access to the dust filter is very difficult. For this we plan to design a small bypass with a dust filter which can be changed easily. We hope that it might be possible to measure the quantity of dust which is present in the primary circuit under operation and measure the particle size and the contamination of this dust.

In closing, I would very much like to emphasize that this program is a co-operative one that has been worked out by all the partners of the HHT-project. The theoretical and experimental work is being done and will be done by the different partners. I would like to thank all the people who have contributed to this program.

Fig. 1. Measurement of adsorption-isotherms (Cs, I)

Fig. 2. Apparatus for sorptions- and desorptions experiments

Fig. 3 . Main circuit of Loop Saphir

Fig. 4 . Test assembly of Loop Saphir

Fig. 5 . Auxiliary circuit of Loop Saphir

Fig. 6 . Particle size distribution of graphite dust

DISCUSSION

L. R. Zumwalt: 1. Do you at present have data on the fission product content of graphite dust from the AVR? 2. Have you contemplated laboratory experiments for determining the absorption isotherms of mixed fission products on graphite dust typical of HHT (AVR)?

C. B. von der Decken: 1. Unfortunately we do not have access to the dust filter in the AVR. Dismantling this filter would interfere with the reactor operation greatly and it is not contemplated in the near future. We have designed a small bypass with an easily removable filter to do studies on this problem in this direction. 2. We will try to do this first since one has to define what will be present in the HTR.

F. H. Neill: What is the source of fission products in your out-of-pile loop?

C. B. von der Decken: The design of the out-of-pile loop has not yet been completed. We have not decided on the kind of source we will use. We will design the loop for using different sources. We will make a decision at a later date and hope that we can profit from the experience of other groups.

A. P. Fraas: Could you do direct contact maintenance on AVR components?

G. Ivens: I am sorry that I cannot give you exact figures concerning the activity level on the AVR components. I can state, however, that maintenance work on components of the fuel handling facility which were in contact with fuel elements and the primary circuit gas could be carried out without any special shielding precautions.

C. B. von der Decken: An examination of the graphite spheres (graphite only) indicated that the highest level of activity resulted from impurities in the graphite. A very low level of fission products was found in the spheres. I am sorry that I do not have any data with me on this subject.

THE HTR DIRECT CYCLE:
ENGINEERING POSSIBILITIES AND MATERIAL REQUIREMENTS

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ABSTRACT

Interest in direct cycle Gas Turbine Power Plants linked to High Temperature Gas Cooled Reactors, stems from the potential savings in electricity generating costs. An expected reduction in capital costs together with a somewhat higher thermal efficiency appears sufficient to justify serious study; whilst, in the longer term, the gradual raising of temperature and efficiency can provide a continuing opportunity for improvement.

As a first step towards evaluating the practical possibilities, recent Dragon Project work has selected a plant layout arrangement and produced a reference design. It has been used as a basis for sizing components, identifying problem areas and assessing the effects of heat exchanger performance, pressure drops, design configurations and gas temperature limits.

When operating with helium at high core outlet temperatures and at the low and representative impurity levels expected in a closed cycle system, there is a need to show that the behaviour of any chosen high temperature material is completely satisfactory. A philosophy regarding the choice of materials and an indication from exploratory test results is given.

The paper gives a view on some of the problems foreseen and some of the proposed general solutions. It concludes that there appears to be no insuperable problem but further study could lead to useful improvements.

INTRODUCTION

The growing interest in a direct cycle gas turbine power plant linked with a high temperature helium cooled reactor stems from the potential savings in electricity generating costs. When compared with the indirect steam cycle system the savings appear sufficient to justify serious study. The expected savings result from the overall

plant and building size, coupled with the somewhat higher thermal efficiency of power generation which can be achieved with presently acceptable operating temperatures. Furthermore, in the longer term, the gradual raising of temperature and efficiency can be seen as providing a continuing opportunity for improvement.

The limiting temperatures are dictated on the one hand by the turbo-machinery design which must operate continuously and reliably; and on the other hand by the reactor core in which fuel and moderator must operate with a high degree of integrity and with a low fission product emission. The supplementary requirements of ducting and controlling the gas flow between them is not without its problems, but in all areas there appears to be a solution available and no immediately restrictive barrier to achieving very adequate working temperatures is evident.

FUEL AND GAS TEMPERATURES

For a first stage exploratory study, a peak nominal fuel temperature of 1300°C is regarded as a suitable limit and in present studies corresponding outlet gas temperatures from peak channels can reach 1100°C .

The mixed gas temperature from all core channels, together with control rod and reflector cooling streams, leads to a turbine inlet temperature of about 967°C (1240°K) and this is theoretically capable of producing a thermal efficiency of power generation of over 47%.

Although the above mixed outlet gas temperature is between 100°C and 200°C higher than currently conceived steam cycle designs the core itself appears virtually identical. Peak fuel temperatures are no more than 50°C higher and this is largely achieved by limiting core power density to about 5.8 MW/m^3 . These are predictably safe values even before taking into account the expected improvements in fuel specifications and performance which could occur during the next 4-5 years.

CORE

Regarding the reactor core, down-flow cooling is now usual in HTR's and it is even more appropriate for the direct cycle system because accidental pressure transients in the direction of flow must not levitate the core (e.g., loss of turbine blading). A coarse flattening of channel gas outlet temperatures by shifting gagged top reflector blocks at refuelling intervals corresponds to a deviation of approximately $\pm 40^{\circ}\text{C}$, about a mean value when carried out at $\frac{1}{4}$ life intervals. The complication of continuously variable gagging is not considered necessary in HTR cores where the fuel can tolerate the temperature swings occurring.

The core inlet temperature in the direct cycle system brings its own particular problems. With good recuperative heat exchangers, core inlet gas temperatures will be likely to exceed 500°C , thus, on-load fuel changing must be designed to suit this temperature in the upper plenum and also around the core support framework, which, together with the thermal neutron shielding, must either operate at this temperature or be cooled by colder by-pass streams. The latter solution has been adopted in the present study and the cooling streams are used subsequently to remove radial reflector heat before entering the under core plenum at a temperature which does not degrade the core outlet gas temperature unduly.

The main penalty arises from the fact that cool by-pass streams do not pass through the recuperator and hence the heat exchanger efficiency is lowered.

In the direct cycle system considered here, fuel element surface temperatures can reach about 1170°C compared with values of about 1000°C in the steam cycle case, but since there is no steam inleakage problem in the direct cycle, graphite corrosion effects can be discounted and the change in dimensional stability of the graphite due to shrinkage behaviour, although worse, is not seen as restrictive.

Control rod cooling streams which pass through the core in parallel with the fuel channels will be largely heated by the surrounding core graphite, thus control rods must be designed to operate at a relatively high temperature, typically 700-800°C.

CHOICE OF LAYOUT

Whilst previous comments outline a general view of the direct cycle which can be backed by thermodynamic evaluation over a range of cases, it becomes necessary to assess the practicability of the concept. It is therefore necessary to choose and develop a reference design which is followed through into practical detail to expose the specific areas where design and material problems exist. This stage is preceded by some evaluation of major components and exploration of layout possibilities. As a result of such work a suitable design philosophy tends to emerge.

A number of basic options are available to the designer; he can choose fully integrated, semi-integrated or non-integrated plant arrangements, and may select either a fixed speed turbo-machinery design with all compressors and turbines on a single shaft, or a split shaft design in which the power turbine operates at fixed speed while the turbo-compressor set may run at variable speed. A further freedom of choice applies in respect of single or multiple turbo-compressor loop arrangements.

In the reference design a split shaft turbo-machinery design was selected for several reasons. Firstly it lends itself to a variety of plant part-load control methods, unlike the single shaft arrangement, which, by virtue of the relatively narrow range of flow variation achievable with fixed speed compressors, is made largely dependent on helium inventory control. Secondly the turbo-compressor unit can readily be mounted vertically to occupy a reactor-vessel wall cavity. Thirdly a separate turbo-compressor unit can be designed to run at speeds substantially higher than the generator synchronous speed. This results in more compact machines and smaller wall cavities.

With regard to plant integration, there can be no doubt that from safety considerations preference must lie with a fully integrated layout, i.e., one in which the reactor and gas turbine plant are housed within a single, enclosing prestressed concrete vessel. On the other hand, the complexity of the gas circuit tends to pose difficult problems for the vessel designer which make it difficult, if not impossible, to achieve axial symmetry in a fully integrated design. The tendency is for vessel configurations to emerge where the tendon arrangements for prestressing are unsatisfactory and the analysis will require the derivation of very complex programmes.

The solution adopted for the reference design houses the reactor, turbo-compressor units and all heat exchangers and coolers in an essentially axi-symmetric concrete vessel, very similar in design to the pod-vessel developed for the steam cycle HTR. The departure from complete integration concerns the power turbines which are located in separate horizontal prestressed concrete vessels positioned underneath the main vessel. By ensuring that the interconnections are arranged to satisfy the same integrity rules as would apply to a single vessel, the arrangement offers the advantage of a fully integrated design.

The arrangement is illustrated in Figs. 1 and 2 where it can be seen that four turbo-compressor loops are used. The power turbine in each loop forms one half of a back-to-back pair so that only two generators are required.

FEATURES

Whilst the safety aspects of a prestressed concrete vessel are well known, some additional points are worthy of mention since they contribute to easy maintenance and simplification of the enclosed components.

Firstly the water cooled liner can be called upon in many areas to apply a useful temperature control. One obvious example is that elastomer seals may be used at penetration plugs giving both flexibility and easy removal.

Secondly the vessel liner requires no expansion features and many of the interconnecting ducts require no insulation. Where higher temperature ducts are required separate insulated liners can be arranged. In the reference design these have been designed as removable assemblies.

Penetration diameters need only be large enough to pass the contained assemblies, gas plenums and flow paths may be made much larger by internal liner design.

In the reference design pre-coolers and intercoolers are each built as one of the penetration plugs and no demountable water joints are inside the vessel. By arranging machine units in self-complete assemblies, mounting may be arranged so that prestressing distortions do not react on machine units.

The general philosophy on maintenance is to withdraw and replace a unit if necessary. Such an exercise should be much faster than the task of opening up the sealed pressure envelope of a non-integrated set.

Regarding the machine frames, they only have to be designed to suit the local pressure differences and since the gas plenums generally lie outside the frame diameters in the vessel walls, the size and handling weight of each assembly is substantially reduced.

The foregoing remarks cover some of the main advantages but most interest will be centred on the problems and one of the most important of these is the achievement of adequate sealing between circuit zones of significant pressure difference. Piston ring type seals have been used in the reference design study but it is not yet known if sonic velocity leakages may arise and cause erosion; some R. & D. work here is clearly necessary.

In the case of potentially hot seals such as those around the H.P. Turbine Shell an excess pressure cold feed is applied between multiple rings after being used to cool the shell and shell hangars. Leaks towards the turbine inlet will degrade the temperature slightly

and can be made good by a small increase in reactor outlet temperature, but leakages downstream towards the L.P. turbine involve an efficiency loss. Where possible, use is made of these flows to sweep an inter-space between hot face duct insulation and cold vessel liner insulation, thus permitting a higher heat flux through the hot face insulation than that lost to heat rejection via the vessel liner.

Similar principles are used for the L.P. turbines but if efficiency losses are to be minimised any cooling stream must be limited to just satisfy its required heat load and unwanted leakages must also be minimised.

The Table in Fig. 3 lists the effect of an evaluation between a theoretical case and the practical case of the reference design where the lower efficiency of the latter is almost entirely due to by-pass cooling streams and leakages.

The use of higher gas temperatures has necessitated a new look at the problem of high temperature insulation, and in particular for the under-core plenum which will experience the highest temperatures in the circuit and which, furthermore, must support the core load. A suitable solution which depends on the use of silicon nitride (Si_3N_4) is illustrated in Fig. 4. Each column of fuelled blocks in the core rests on its own graphite support column which is headed by end reflector blocks and all of these members can be removed if required. The columns are seated on slabs of foamed silicon nitride which are mounted in turn on a series of thermal neutron shield plates. A reasonably good conductive path for heat flow is finally made by a layer of lead and steel shot compressed to give a sufficiently flat upper face for near contact with the lowest shield plate. If necessary the silicon nitride can also be removed after a graphite top layer has been cleared.

Although the thermal conductivity of Si_3N_4 is not as low as one would like and a knowledge of its conductivity in the foamed form is still lacking, it appears to have very attractive design properties such as a very low expansion coefficient (about half that of graphite),

a strength much higher than graphite and a high thermal shock resistance. Its behaviour under irradiation from the limited data available to date, looks quite satisfactory for this application. Its stability in the reactor coolant is theoretically good up to at least 1000°C , but further work must be undertaken to prove its suitability on all counts.

If there are no problems with the use of silicon nitride it will be very suitable for use as a high temperature insulation inside metal duct liners. Should it be felt necessary to provide a metal covering to the surface then methods of attaching a sheet layer, such as molybdenum, can be arranged.

CONTROL

Three basic methods are available for load control:

1. Inventory Control: Where the system pressure is varied to suit the load required. This is the most efficient method but the speed of response is dependent on the time required to change pressure.
2. By-pass Control: There are various positions in the circuit where a by-pass can be used for control purposes. The response can be rapid but the thermal efficiency of the plant at lower loads is poor.
3. Temperature Control: By moving reactor control rods this method can be relatively fast if high rates of reactivity change are accepted. The efficiency at part-load lies between the two previous cases.

For maximum flexibility some combination will very likely be used. If, for example, pressure level is set for the peak load expected then limited variations in demand can be handled by temperature control with little loss of efficiency. If quicker response is required than can be achieved by control rod speed then by-pass control is needed, but the efficiency loss will be greater.

Loss of generator load is a more exacting problem and in this event a quick acting turbine by-pass, together with a blocking of the normal flow path through the turbine is needed. A typical overspeed rate is 10% per second so that full diversion of flow in about half a second is required. A solution to this problem can be achieved by the use of multiple poppet valves each held shut by an overpressure bellows-sealed actuator through an appropriate linkage. By releasing actuator pressure the linkage collapses and system pressure blows each valve open. Twenty-four 10 cm diameter valves built into each L.P. turbine stator casing provide the diversion in the reference design case. The closing of the path through the turbine is achieved by flaps which are linked to the respective actuator and which fold in from the shell to expose the gas path through each corresponding poppet valve. The gas temperature at L.P. turbine entry is about 760°C and to limit the temperature of the diverted gas, a graphite heat sink is interposed which also provides a suitable pressure drop to minimise pressure transients and the consequent tendency to speed up the H.P. turbine.

Other by-pass valves do not seem to involve any real difficulty. A clear specification for their duty is dependent on the completion of control studies but they may be typically as follows:

- (i) Small by-pass control valves between H.P. compressors and H.P. turbine to give temperature control of H.P. turbine inlet. One need for this valve would be in an early shut down condition when H.P. turbine load would be small and a low turbine inlet temperature would be needed to prevent the outlet temperature from being too high.

It can also be used for quick load reduction.

- (ii) A by-pass valve from the cold H.P. side of the recuperator (130°C) to the hot L.P. side to produce a limited flow diversion will effectively cool the gas entering the recuperator from the L.P. turbine exhaust. If this is opened on loss of load it will tend to collapse the pressure differences between H.P. and L.P.

sides of the loops whilst at the same time reduce any temperature transient to the recuperator tubes. A valve diameter of about 20 cm would serve two loops.

- (iii) A control by-pass across the L.P. turbine may be necessary for start-up purposes. It may also be useful for load control trimming, but since the inlet temperature would be about 760°C its duty would be less arduous if the latter purpose were unnecessary. The only design problems seen are those of arrangement so that any sensitive actuator features are kept clear of the high temperature gas stream.
- (iv) Circuit Stop Valves are necessary if loops are to be closed down while the others are working. In the reference design a pair of loops must be shut down together since they are tied to one generator, thus the plant can be run at 50% load on the remaining two loops.

The Stop Valves in the reference design are placed between the L.P. compressor outlet and intercooler inlet. The temperature will be about 130°C when open, but when closed any leakage gas will be at about 40°C . The pressure differences across the closed valve will be equivalent to the core Δp which is about half an atmosphere. Elastomer seals are possible for these valves in view of the low temperature, it should be noted however that under closed loop conditions the pressure throughout will reach reactor plenum values so that all pressure vessels must be designed for full system pressure.

MATERIALS

It could be simply stated that the only requirement concerning materials is that they should retain their structural integrity during the whole of their service life. This integrity is dependent on both mechanical and chemical factors. In the direct cycle HTR it is the higher coolant temperatures of both core inlet ($500\text{--}550^{\circ}\text{C}$) and outlet ($1000\text{--}1050^{\circ}\text{C}$) as compared to the steam cycle HTR that dominates the

choice of materials. Attention must, therefore, be directed to the problems of ducts, liners and insulation, particularly those that are built permanently into the plant and must therefore have a service life of 250,000 hours. The final solution becomes a compromise between design philosophy, materials selection, fabrication influences and cost.

Experience in the operation of nuclear installations has emphasised the importance of component replacement and plant maintenance. The design proposals have therefore involved the principle, where practicable, of component replacement, including members which are normally permanent such as ducts and liners, particularly those operating in the high temperature areas of the plant. In all such designs dimensional stability is of prime importance, and great care must be taken in the choice of metals and alloys to ensure freedom from distortion under actual service conditions. Such dimensional stability demands metallurgical stability, and for temperatures above 500°C the choice becomes limited, especially if significant stresses are involved.

Most conventional engineering metals used at temperatures in the range 500-750°C are based on alloys of iron, nickel and chromium. At these temperatures atomic re-arrangement (diffusion) can take place resulting in the formation of a new phase, for example, the sigma phase in certain austenitic heat resisting steels. These phases (precipitates) are formed initially at the grain boundary, they can also form within the grain. Whatever the nature of the phase change it is generally accompanied by a change of volume which produces local strains. Unless the whole component is operating under truly isothermal conditions - a condition rarely attained in service - the phase change does not take place homogeneously, significant strains are set up, and distortion is inevitable. This is especially true of flanges and other components, where the maintenance of circular geometry is vital, operating at a temperature in excess of that where atomic re-arrangement can take place.

The use of a thermodynamically stable alloy would of course eliminate this phenomenon completely. However, the relatively poor mechanical properties of such alloys inhibits the designer to such an extent that some degree of metallurgical instability must be accepted in order to obtain adequate strength at operating temperatures. Attention must also be directed to the choice of alloying additions and to the nature of the precipitates that are formed. Very long periods at temperatures approaching the useful limit of the alloy (from the mechanical properties point of view) can lead to continuous grain boundary precipitation. These precipitates may well have entirely different corrosion characteristics to that of the parent metal, and if for example the precipitate is attacked premature failure will occur.

A consideration of the above factors suggests that the stabilised austenitic stainless steels (provided they are completely free from sigma phase formation) the Inconels, the Incolloys, IN 102, and for temperatures in excess of 800°C, molybdenum should be appropriate.

The use of helium avoids any gross corrosion problem. However, the effect of the residual impurities are such that they must not be overlooked. Experience in the Dragon Reactor Experiment has shown that, under operating conditions impurity concentrations in the range 1/10 to 1 vpm total impurities can be expected in a direct cycle plant. Although these originate primarily from air and moisture adsorbed on to the core and the reflector graphite - only a small fraction desorbes from metals - they soon equilibrate with the large volume of hot carbon and graphite in the core. This equilibrium results in a mixture comprising hydrogen, water vapour, carbon monoxide, carbon dioxide, methane and nitrogen, the final product having an oxidation potential equivalent to a hydrogen - water mixture of $H_2:H_2O \approx 10:1$. With the higher temperatures involved in the direct cycle system the ratio is likely to be somewhat higher, probably approaching 100:1.

We have therefore as coolant, a gas, predominantly helium, at 50-100 atmospheres pressure, at temperatures up to 1050°C containing traces of impurities which from the chemico-thermodynamic point of view are reducing to the elements nickel, cobalt, iron, copper, tin, molybdenum, tungsten, etc., but oxidising to chromium, aluminium, titanium, niobium, etc. The same thermodynamic principles apply to carburisation due to the presence of methane and carbon monoxide.

The conventional high temperature nickel/chromium alloys are all dependent on an oxide film for protection from their normal environments. As it was thought that the HTR environment might not be conducive to the formation of such protective films, experiments were put in hand to study the phenomenon. Exploratory results, designed to assess the general significance of the problem have shown that solid solution alloys, e.g., the austenitic stainless steels and IN 102 (Figs. 5(a) and (b)) are not significantly attacked, whereas the precipitation hardened nickel chromium alloys undergo serious internal oxidation (Fig. 5(c)). Selective oxidation of the chromium, aluminium and titanium does not give a protective film and attack proceeds by internal oxidation which is diffusion controlled. Such attack is particularly dangerous where the elements that are oxidised play a vital role in the strength of the alloy, as is the case with the "Nimonic" alloys and related materials.

Further work is required to define the working limits of those materials that have shown promise in the exploratory tests, and both stressed and unstressed corrosion tests are in hand. As the attack is basically diffusion controlled it should be amenable to prediction by studying its effect at higher temperatures and analysing the results using the accepted laws of diffusion and reaction rates.

It may well be possible to protect those alloys that are attacked by appropriate coatings. Two principles seem worthy of study. One, by using nickel or cobalt which being noble to the environment would limit oxygen and carbon concentration and therefore reduce diffusion

rates: the other with chromium or possibly aluminium containing coatings which might form a protective oxide film.

Molybdenum and its alloys deserve special mention. Thermodynamically the metal is stable to the HTR environment at temperatures above 400-500°C. Furthermore at the lower temperatures, where oxidation does take place, MoO_2 alone will be formed, which unlike MoO_3 is not volatile. Oxidation rates at these temperatures are very low and since the molybdenum would not be used for components operating continuously below 700°C oxidation would not appear to present any problem. Furthermore, since diffusion rates in molybdenum are negligible at temperatures below 1000°C carburisation or internal oxidation of alloying elements such as zirconium should not prove significant. No attack on either the unalloyed metal or on TZM (Fig. 5(d)) at temperatures up to 850°C was observed in any of the exploratory experiments referred to above.

Because of the specific thermodynamic characteristics of the impurities it should be possible to develop high strength alloys using only those elements that are stable to the HTR environment. For example nickel, cobalt and iron can be considered as forming the basic alloy with tungsten and molybdenum providing solid solution hardening. Copper and tin would be expected to provide precipitation hardening characteristics. Such alloys being thermodynamically stable both with respect to oxygen and carbon would not be expected to deteriorate in service, any limitation being confined to their mechanical properties.

The absence of protective surface films focusses attention on friction and seizure between mating parts which might cause maintenance difficulty. Experience in the Dragon Reactor Experiment has shown that with an appropriate design philosophy, serious problems can in general be avoided. During component development work for the Dragon Reactor it was found that a simple correlation existed between the temperature at which a polished metallographic section of the metal started to show thermal etching in vacuo, and

the temperature at which static seizure takes place. This test is very easily carried out and provided an excellent guide as to the maximum temperature at which any particular metal or combination of metals could be used without significant danger of seizure.

Where mechanisms or machine motions are involved, temperatures can usually be kept low enough to adopt the already proven methods of lubrication even if cooling must be arranged.

Sliding motions caused by expansion will require some attention where they lie outside the present range of experience. Demountable duct liners and piston ring seals may fall into this category but wear resistant surface coatings on a stable base material can be expected to meet the needs, particularly when the design conditions are eased by reducing temperatures by suitable heat breaks. An appropriate application of a dry lubricant such as molybdenum disulphide (MoS_2) can also be expected to play its part. In general the temperature of such features can be maintained below 600°C without seal cooling flow, design arrangement can also ensure that any leakage will involve the passage of cool gas, and hence lower temperatures would apply.

Where structural shells are no hotter than $300\text{--}400^\circ\text{C}$ then conventional mild steels with good fabrication qualities would be chosen. Up to about 550°C low alloy steels, such as the plain molybdenum, chrome molybdenum and nickel alloy steels would appear to be appropriate. This would include recuperator tubes, turbine shells and much of the removable duct shells carrying internal insulation.

Where the temperature of the material is higher, up to $750\text{--}800^\circ\text{C}$ a nickel base alloy such as IN 102 looks attractive whilst satisfying the previous arguments on stability.

Higher temperature needs, such as in the H.P. turbines, are more likely to require smaller members such as stator blade carriers and shell linings, and in the hottest zones molybdenum TZM will more than

satisfy the requirement. However the use of more conventional high temperature materials such as the Nimonics are not ruled out above 750°C since the problem of attack would seem to be amenable to protection by an appropriate coating.

Similar arguments apply to the hotter turbine rotor disc materials. The use of disc cooling can ease the limitations but the extent to which this compromise has economic benefit remains to be evaluated in relation to the whole plant performance.

CONCLUSIONS

The previous sections give a very limited account of present thoughts and they follow from very recent and incomplete work. They are however beginning to take into account the more practical aspects of the HTR Closed Cycle System for large scale power generation.

The reference design chosen and the parameters used are in many ways arbitrary but they result from the steam cycle HTR studies and an appreciation of the extent to which they can be modified to produce a viable closed cycle design for technical evaluation.

Although there appear to be no insoluble problems, further study on arrangement and component design could lead to a number of basic changes either to circumvent a problem or to simplify the solutions.

ACKNOWLEDGMENTS

The authors wish to record their appreciation for the contributory work of their colleagues and for the interest and assistance given in many ways by other organisations.

1. REACTOR PRESSURE VESSEL
2. POWER TURBINE PRESSURE VESSEL
3. STARTER
4. LOW PRESSURE COMPRESSOR
5. INTERCOOLER
6. HIGH PRESSURE COMPRESSOR
7. HIGH PRESSURE TURBINE
8. LOW PRESSURE TURBINE
9. RECUPERATOR
10. PRECOOLER
11. BELLOWS EXPANSION JOINT
12. REACTOR CORE
13. REACTOR INLET PLENUM
14. REACTOR OUTLET PLENUM
15. FUELLING STAND-PIPE POSITIONS
16. LOOP BLOCKING VALVE

1000 MW (E) DIRECT HELIUM CYCLE HIGH TEMPERATURE REACTOR
GENERAL ARRANGEMENT VERTICAL SECTION

FIG. 1

1. REACTOR PRESSURE VESSEL
2. POWER TURBINE PRESSURE VESSEL
3. RECUPERATOR AND PRECOOLER CAVITY
4. TURBO-MACHINERY AND INTERCOOLER CAVITY
5. FUELLING STANDPIPE POSITIONS
6. DUCT FROM H.P. TURBINE
7. DUCT TO RECUPERATOR
8. L.P. TURBINE
9. SHAFT SEAL
10. SEAL LEAK COLLECTION SHELL
11. GENERATOR
12. SPACE FOR HOUSING POSSIBLE SHUT-DOWN COOLING LOOPS AND OTHER PLANT ITEMS

1000 MW (E) DIRECT HELIUM CYCLE HIGH TEMPERATURE REACTOR
GENERAL ARRANGEMENT. PLAN AND SECTION THROUGH POWER TURBINES

GAS TEMPERATURES PRESSURES AND FLOWS IN THEORETICAL AND PRACTICAL CYCLE CALCULATIONS					
POINT IN CYCLE (SEE DIAGRAM)	THEORETICAL CYCLE WITHOUT BYPASSING OR HEAT LOSSES (TOTAL FLOW 885 kg/sec IN 4 LOOPS)		PRACTICAL CYCLE WITH GAS BYPASSING AND HEAT LOSSES TAKEN INTO ACCOUNT		
	PRESSURES ATA	TEMPERATURES °C	PRESSURES ATA (1)	TEMPERATURES °C	GAS FLOWS (TOTALS IN 4 LOOPS) kg/sec
①	59.04	967	59.04	967.00	926.00
②	57.49	780	59.62	762.07	943.31
③	19.68	568	19.61	548.90	954.41
④	18.70	174	18.79	160.93	954.41
⑤	18.53	37	18.53	37.00	954.41
⑥	55.55	150	55.55	150.00	954.41
⑦	53.15	37	53.15	37.00	954.41
⑧	60.00	130	59.95	129.95	919.60
⑨	59.54	519	59.60	512.15	815.04 (2)
⑩	59.04	967	59.10	1,020.12 (3)	815.04 (2)
HEAT INPUT	2,156.3 MW		2,205.2 MW		
GROSS ELECTRICAL OUTPUT (4)	1,023.9 MW		999.1 MW		
GROSS PLANT EFFICIENCY (4)	47.472 %		45.245 %		
<u>NOTES</u>					
(1) PRESSURE LOSSES CALCULATED SUBSEQUENTLY WERE 10-15 % HIGHER THAN SHOWN HERE					
(2) FLOWS SHOWN FOR POINTS ⑧ AND ⑩ ARE THROUGH THE FUELLED CHANNELS ALONE					
(3) GAS TEMPERATURE AT EXIT FROM THE AVERAGE FUEL CHANNEL.					
(4) GROSS OUTPUT AND PLANT EFFICIENCY BASED ON OUTPUT AT GENERATOR TRANSFORMER TERMINALS BEFORE DEDUCTING STATION LOADS					

FIG. 3 THEORETICAL AND PRACTICAL CYCLE DATA

Fig. 4 Core Support Columns

(a) Stainless Steel 347 type 18/8 Nb 750°C

(b) Nickel Alloy IN 102 750°C

Fig. 5(a) and (b) Metallographs of Etched Sections Showing Attack of Unstressed Specimens Exposed to Helium Containing 250 vpm CO 250 vpm H₂ and 50 vpm H₂O for 3,000 hours

(c) Nimonic 105 850°C

(d) Molybdenum Alloy TZM 850°C

Fig. 5(c) and (d) Metallographs of Etched Sections Showing Attack of Unstressed Specimens Exposed to Helium Containing 250 vpm CO 250 vpm H₂ and 50 vpm H₂O for 3,000 hours

DISCUSSION

R. Huddle: I should like to add a word to what George Lockett has said. The design presented was developed on the basis that it was going to be detailed and built. It is not an academic design - each and every part can be made from materials that exist today. Some may need further proving, but it is a fractional perturbation, ready for detailing and construction. Regarding materials, emphasis has been placed on stability and reliability. The only really unconventional materials are silicon nitride (which incidentally is marketed here by Haynes Stellite) and molybdenum - two materials used for ducts and liners. We can build it now - let's hope we get an opportunity to do so.

M. Bender: Considering the relatively low thermal efficiency (45%), what is the economic incentive for gas turbine systems? Does the target have to be 50% efficiency or more?

G. E. Lockett: There is no specific target efficiency. The results of studying this reference design show that the theoretical cycle without bypassing and heat losses, compared with the practical case, reduces the plant efficiency from 47.47 to 45.24%. The economic incentive is to reduce the generating cost by reducing capital cost and fuel cycle cost whenever possible. The reduction in plant and building size compared with the steam cycle is expected to do this without any emphasis on efficiency. However, any improvement on thermal efficiency will permit a given power output to be obtained from a smaller reactor core. This early study is attempting to get an appreciation of practical penalties and to expose any previously unseen problems.

K. O. Hintermann: In fossil-fueled, closed-cycle gas turbine plants the air heater takes almost half of the capital cost. Such a plant is roughly comparable in generating cost with a steam cycle. In a nuclear gas turbine plant the "air heater" is replaced by the reactor. Therefore, there must be an economical incentive provided the reactor is not very much more expensive.

M. Dalle Donne: What material did you consider for the ducting at temperatures of about 1000°C, which is the helium temperature at the reactor outlet that you assumed?

G. E. Lockett: We have considered the use of silicon nitride mostly on the inside of the tubular metal shells. The core support mounting is also based on the use of silicon nitride.

W. Twardziok: Is emergency cooling accomplished with the high pressure turbo set? How can you separate the systems?

G. E. Lockett: There are four turbo-compressor units feeding two back-to-back pair of power turbines, thus there are four parallel paths, but there is a linking point between the back-to-back power turbine exhausts. For normal operation it becomes effectively a two loop system with two turbo-compressor sets in each. For load outputs of 50% or less, it is possible that the plant would be run using one generator only with its associated two turbo-compressor loops. To satisfy the problem of emergency cooling, failure of one of the turbo-compressor units should not prevent the remaining unit from continuing to run - at least to an adequate extent. The behavior has not yet been studied in any depth, but it would be necessary to close the blocking valve in the failed turbo-compressor loop to prevent backflow. There will remain, however, a bypass flow route around the turbine of the good turbo-compressor unit. This may not be too important if appropriate resistance is engineered into the leaking point between the two power turbine exhausts. It is possible to reduce the temperature of the bypass flow by partly opening the control valve between the high pressure compressor outlet and the high pressure inlet of the failed unit. There remains, of course, the possibility of starting up the other part of the system as convenient in preference to using one-half of the failed loop pair alone. Further questions arise as to whether an attempt should be made to keep the generator running at low output in order to satisfy the base load. From the operational point of view, it would be more straightforward to use both generators even at power outputs of below 50% so that a failure in one-half of the system will allow it to be fully shut down and allow the other half to continue generating without interruption.

W. Twardziok: What is the overspeed of the power turbine when you have a load decrease?

G. E. Lockett: In the event of load loss, the power turbine overspeed need not exceed 10%. A typical acceleration of the coupled power turbine and generator rotor being about 10% per second. There is no difficulty in opening bypass valves quickly to divert flow around the power turbine if multiple poppet valves are used as shown in the attached figure. These can operate to divert flow in 30 to 50 milliseconds. If a pause of 0.2 seconds is made before tripping the bypass and a liberal allowance made for incomplete bypassing, an overspeed of about 7% might be reached.

PRESSURE VESSEL TECHNOLOGY

AND SAFETY TOPICS

(Session IV)

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SAFETY ASPECTS OF HIGH TEMPERATURE REACTORS

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ABSTRACT

The methods of assessing the fission product release and the graphite corrosion in a High Temperature Gas Cooled Reactor are reviewed. They are applied to a notional core to demonstrate the available margins and to indicate the priorities of information.

These priorities are shown to be the objectives of the fission product and corrosion work underway in the Dragon Reactor Experiment programme.

INTRODUCTION

Safety arguments for reactors are developed alongside the technology into what becomes a rigorous examination of the principles and potential of the system. In a restrictive sense this is directed towards assessing potential hazards. However, the technical penetration required provides a unique method of assessing also the standards required of the experimental work, the calculational methods, quality controls and the operational practices.

The object of this paper is to examine what these requirements are for the first HTR in the UK and to relate them to the present status of fuel development as described by Dr. Graham, the fission product release studies (Dr. Flowers), core design (Mr. Smith) and management of the reactor experiment as described by Mr. Chapman.

The paper deals first with the best present estimates of fission product release and corrosion in design and transient conditions, explores in each case the limitations and available margins which arise from present data and then relates these design and operational restrictions to the Dragon Reactor Experiment and its attendant research work.

METHODS OF ASSESSMENT

Safety begins at the design stage in a way which is more obvious than for previous gas-cooled reactors. Designers, safety assessors and operators demand to know the limits imposed by the particle coatings and the graphite can which constitute the barriers to fission product release unique to an HTR because these appear at first sight to be vulnerable to imperfections in manufacture, to corrosion and to mechanical damage.

The array of accident situations which are judged necessary to be considered is summarised in Table 1.

The starting point is a reference core devised to represent as realistically as possible the equilibrium composition of fuel channels of various ages, nominal temperature distributions and fission product

Table 1. Reactor Conditions to be Analysed

Situation	Typical Faults
<u>Normal Operation</u>	Radiation doses not exceed some arbitrary fraction of the ICRP recommended annual dose.
<u>Category A Accident</u>	<p>Could occur several times in a station life. Radiation doses do not exceed ICRP recommended annual doses.</p> <p>Fuel pin failures leading to small scale increases in activity releases.</p>
<u>Category B Accident</u>	<p>Probability of occurrence no more than once in a station life.</p> <p>(a) Pressure circuit breach of about 45 square inches, equivalent area.</p> <p>(b) Coolant starvation of a single channel with delayed trip.</p> <p>(c) Failure of a single boiler tube with feed water and reactor trip.</p>
<u>Category C Accident</u>	<p>Probability of occurrence is so low as to be discounted. Analysis of safeguards and consequences of accident necessary to support the judgement of this probability.</p> <p>(a) Boiler tube failure with simultaneous circuit depressurisation through relief valves.</p> <p>(b) Major breach of primary circuit.</p> <p>(c) Gross overheating of several fuel columns.</p>
<u>Circuit Access</u>	<p>(i) Circulator inspection or removal. Once in 2-3 years.</p> <p>(ii) Inspection and repair of static installations such as hot and return gas ducts, hot plenum insulation. Once or twice in station life.</p> <p>(iii) Large scale installations designed to be removed with accompanying long shutdown (boilers). Very infrequent.</p>

inventories. Onto this core are imposed the estimated peak systematic and peak random temperatures which arise from flux tilts, changes in gap sizes during irradiation, coolant by-passing, etc. The fuel compacts are characterised by the as-produced uncoated fraction of particles and the current particle failure limits in terms of burn-up, dose and temperature.

The route for estimating the fission product release is shown in Fig. 1. The data requirements from the core design are first specified and the calculations are entered with estimations of single channel releases. At this point the data is used to calculate the service failure, if any, of particle coatings which together with the production quality constitute the source terms. Following the estimation of fission product inventories at each time step, the metal release fractions are estimated using a model of the diffusion and evaporation processes in the fuel and graphite regions of the channel. Iodine and fission gas releases are similarly calculated with the important difference that hold-up is controlled almost entirely by diffusion in the kernel material of the particles.

If abnormal conditions are to be examined, the transient data are fed into the channel data, and in the course of the calculation, the nuclear data, the diffusion coefficients of the fuel and graphite regions and their temperature and concentration dependences are entered.

After the stage of axial integration, a comparison of various channel concepts in terms of their fission product release characteristics can be made, which returns to the designer and the fuel manufacture as a coarse setting of performance and quality limits.

The next stage is the representation of the complete equilibrium core by weighting each characteristic channel release and this leads to the comparison of tolerable release rates in terms of curies per year or effective release-to-birth ratio from the core.

This comparison allows the setting of closer limits on the channel performance and the fuel specification. It provides a quantitative

assessment of the merits of each type of coating and the requirements of the experimental programme to determine diffusion and evaporation data. Finally, it provides an estimate of available margin, defined as the ratio between tolerable and predicted release, which can be used as an optimisation parameter, or referred back through the channel performance to the particle quality control and to the operation of the reactor.

The corresponding route for corrosion estimation is shown in Fig. 2. The same reference core data is used as input but it is more convenient to characterise the core by graphite surface zones each with a particular axial profile. Effectively, a histogram of surface temperatures is used in the calculation of integrated core chemical reactivity. The tolerable total water inleakage is set by the tolerable corrosion at the hottest graphite surface and this is entered together with the integrated core reactivity into the estimation of tolerable inleakage.

The output provides an estimation of the available corrosion margin at any time and position in the core which, because the reactions are first order to a close approximation, is simply expressed as a tolerable exposure in micro-atmosphere days.

At the high water concentrations which could occur following a boiler tube failure, the steam graphite reaction rates are determined by the transport processes of reactants and reaction products within the graphite as well as the reaction rates. Therefore it is necessary to calculate the chemical reactivity of each axial zone accounting for these processes at various times during the incident to take account of the changing distributions of heat and temperature and the alterations of coolant composition and mass flow. This route is embodied in the TUBER code.

APPLICATION TO A REFERENCE CORE

The data for a particular core design is outlined in Fig. 3 and Table 2. It was chosen deliberately as a non-optimised design to

Table 2. Reference Core Data

Core Power	1500 MW(th)
Power Density	7.5 kW/litre
Core Height (fuelled)	500 cm
Core Diameter	768 cm
Number of channels	4,630
Age factor	1.35
Radial Form Factor	1.37
Channel Diameter	6.5 cm
Outer Fuel Pin Diameter	5.8 cm
Inner Fuel Pin Diameter	2.5 cm
Graphite Tube Thickness (inner and outer)	0.5 cm
Fuel Density	1.0 g/cm ³ (heavy metal)
Peak Channel Power (new fuel)	0.6 MW
Peak Channel Dwell Time	730 days

Core Zones	Radius cm	Number of Channels	Power Factor
1	55	96	1.0
2	111	288	0.96
3	166	481	0.895
4	221	670	0.79
5	276	861	0.67
6	384	2,234	0.45

Table 3. Available Margins for Reference Core

Nuclide	Continuous Release (Stack 10 m No Filtration)	Access (1 week, Shielding x 10 Non-Uniform Distribution)	Depressurisation through 45 sq in Hole (Ground Level)	Failed Boiler Tube Depressurisation through Safety Valve Stack Discharge)
Caesium-137	52	2.2	330	>300
Strontium-90	30	-	4.1	75
Iodine-131	0.9	2.3	2.0	25

demonstrate how the release and corrosion estimates influence the design and data requirements. The service failure of fuel was estimated from the total fission gas and carbon monoxide pressure developed at a particular temperature and burn-up, using as a criterion of failure the time at which the compressive stress in the silicon carbide diminishes to zero. This proportion was added to the assumed production defects and shown in Fig. 4 which shows this defect fraction in the hottest 20% of the peak channel. The remainder of that channel and channels in each zone were examined in the same way.

The estimating route was followed down to the fractions of caesium-137 and strontium-90 released per year and the Release to Birth Rate ratio for Iodine-131. The results are shown in Figs. 5, 6 and 7, where these fractions are plotted as functions of the start-of-life defect fraction. At the $5 \text{ in } 10^4$ defect fraction, the releases due to service defects are superimposed.

This brings out clearly the fact that end-of-life failure to levels of 2-3% of the fuel particles has a relatively small effect on the release to the circuit of caesium and strontium. This is due to the transmission delay imposed by the graphite sleeve.

These predicted releases are related to the tolerable levels in terms of the available margins in Table 3, where it can be seen that these are considerable margins even for this unoptimised design and particle failure fraction. Only in the case of the continuous release of iodine-131 is the margin less than one. In analysing the tolerable level, an effective stack height of 10 metres was assumed, with no filtration and no decontamination in the helium leak path between the primary circuit and the stack exit. The results from the Dragon Reactor leakage and activity measurements are directly relevant and will be supplemented with deliberate leak behaviour experiments. Therefore apart from the development of fuel and production methods, engineered safeguards are available to improve this margin 20 or 30 times without exceptional expense.

This factor is necessary to allow for any conservative downwards adjustment of the tolerable continuous release to atmosphere.

Corrosion Analyses

The design criteria of corrosion are the tolerable graphite surface attack or loss of section at the hot spot, the bulk corrosion and loss of mechanical strength of the graphite below 800°C and the corrosion of the fuel matrix in regions where the graphite temperatures are low enough to allow penetration of reactant. The region of greatest risk is where the sleeve temperatures are at 750°C and the fuel 50°C or more hotter.

Of these criteria, in normal operation, the present view is that the loss of section at the hot-spot is the most severe. The limit is set at 100 mg/cm², corresponding to an average removal of 0.6 mm from a 5 mm tube, with local pitting reaching up to approximately 2 mm.

This limit, therefore, together with the temperature distributions of the reactor zones are entered into the corrosion calculation route. The results of this calculation are shown in Table 4. The graphite chemical reactivity was assumed to be that measured for Gilsocarbon (1.2×10^{-4} mg cm⁻² h⁻¹ μ atm at 1273°K). The fact that the calculation assumes that all the water reacts with the core, whereas some 20% is removed by the plant, accounts for the margin available at the hot-spot, which was taken to be at the peak random temperature of the centre channel. The available margins in the peak channels of the outer zones always exceed this value, taking the longer dwell times into account as well.

The reactor can be operated with an integrated water inleakage of up to 300 kg/year before it is necessary to discharge centre zone channels of high burn-up because of corrosion limitations.

The extreme case of a spontaneous boiler tube failure which could inject water at rates between 10 and 40 kg/s up to a total of 2,000 kg has been considered by Kinsey and Römberg. Because of the rapid reaction with the core, no alteration of the graphite temperatures was assumed, and no fast water detection and dumping taken into

account. The limit of 2,000 kg was chosen arbitrarily; obviously the total removal of graphite under these assumptions is approximately linearly proportional to the total reactant. The total graphite removal was 1,800 kg with a maximum linear attack at the hot-spot of about 80 mg/cm^2 . The maximum attack of the particles by water penetrating the sleeves in lower temperature regions was the removal of about 2 mm of the matrix material and 10 microns of the outer pyrocarbon of the particles in that region. The reduction of the contaminants by the clean-up plant to 1% of their peak values took 8 hours.

Two conclusions emerge from this work. First, the attack is nowhere severe enough to cause structural failure of the fuel pins. In fact a large proportion of the core remains within the tolerable corrosion margins, and could be operated. Second, the total removal of caesium and strontium is within the available margin of release through a relief valve, although the complete case of subsequent depressurisation has not been analysed.

DISCUSSION

The fission product release and corrosion estimations for an HTR were singled out of the wider safety analyses because they represent the areas of proof which differ most from other gas-cooled reactors. A measure of confidence is created by the fact that the input data is being obtained from measurements in conditions and geometries which do not involve large extrapolations. It is, however, worth examining the effect of errors in this data.

If the value of the graphite diffusion coefficient for strontium, for example, were increased by a factor of $\times 3$, the released fraction per year would increase by a factor of about 1.5, and if, separately, the evaporation coefficient were increased by the same factor, the release would be increased by $\times 2$. Both are required, therefore, to about the same accuracy as the start-of-life source term. For both these metals, the effect of end-of-life failure of fuel is shielded by the delay time in the graphite and a factor of $\times 3$ at 80%

irradiation for example is judged not to cause an increased release of more than 20%.

However, the iodine release is more sensitive, and the location of defective fuel to within a small number of channels (say 25) would provide the operator with precise knowledge of the end-of-life behaviour for which predictions must incur penalties in terms of tolerable fuel life.

An important parameter in the estimate of iodine activities is the temperature dependence of release. A value of 68 kcal/mol for the diffusion coefficient of iodine in the fuel grain is based on the measurements of xenon diffusion in single crystal UO_2 . The effect of changing this value is shown below on the release from the peak channel of the core defined in the previous section.

Activation energy	40	68	80 kcal/mol
Iodine-131 release from channel at 20% irradiation	140	120	80 curies/s $\times 10^{-9}$

The corrosion predictions are linearly proportional to the water ingress rate assumed and the in-pile graphite reactivity - and logarithmically dependent on temperature. However, the adequacy can be checked directly by injection experiments in the Dragon Reactor, and the designed distributions of the graphite temperatures which are monitored in the core. A particular example is the application of the corrosion estimates to the Charge 3, Core 6 which predicted a core chemical reactivity of 2.4 h^{-1} , assuming all the graphite to be G5 material, and 7.2 h^{-1} for Gilsocarbon graphite, using out-of-pile oxidation data. The measured value was 4.0 h^{-1} . The majority of the graphite was, in fact, Gilsocarbon and the result is reasonably good when a probable error of $\pm 50^\circ\text{C}$ at the hot-spot is taken into account. Previous correlations for experiments in Charges 1 and 2 have been reported showing the same or better agreement.

Finally, the interaction between corrosion and fission product release is an area where calculations require close support from

experiments; this includes the removal of plated-out metal fission products and iodine from primary circuit surfaces during a boiler tube failure accident. The available margins in these cases are large and reproducibility better than a factor of 5 is probably not important. The planned work at Dragon is mentioned below.

DRAGON REACTOR EXPERIMENTS

The instrumentation and sampling systems in use include the following:

- (a) the purge sampling system which enables the fission gas activities to be monitored from each of the purged fuel elements,
- (b) two fast purge sampling lines through which the gas transit time is about 2 seconds,
- (c) the Dracule probe which enables the core exit gas to be sampled at rates up to 100 litres/min and in which plate-out probes of various materials can be inserted,
- (d) the fast response loop for sampling the bulk coolant activity and for the injection of gaseous impurities,
- (e) the centre-spike injection for single element corrosion tests and tests of the influence of corrosion on the release of metal fission products,
- (f) various coolant sampling filters.

These are shown schematically in Fig. 8.

The facilities have been installed progressively with the objective of using the reactor to prove the prediction methods and two exercises are at present in progress.

The first is to log the exposure of each element to oxidation by synthesising the core chemical reactivity R_c from the temperature scans and the reactivity of each graphite component. The limit of impurity injection tolerable at any time is then estimated. Injection into the bulk coolant provides a measured value of R_g and

injection into the centre element of continuous high concentrations (1,000 μatm) provides an in-pile calibration of the oxidation rates by the subsequent measurements of weight loss and corrosion profile. This facility is used simultaneously to simulate the leakage of coolant through fuel pin end-cap features and the consequent corrosion of the matrix.

The reactor is now operating with new, uncontaminated heat exchangers and with surfaces elsewhere well monitored for deposited and induced activity. This represented a good starting point for the second exercise, the testing of a circuit activity control method. The procedure is:

- (a) to collate the information available from fuel production concerning the quantity and distribution of exposed fissile material in each element,
- (b) to relate these source terms by the in-pile purge sampling of fission gases,
- (c) to calibrate this gas sampling with reference fuel elements containing a known proportion of uncoated kernels,
- (d) to predict the metal releases into the graphite and coolant from the reference element,
- (e) to check these predictions for both the reference and certain other elements by post-irradiation analyses.

These exercises enable both the corrosion and activity routes to be checked. The first results in both are encouraging.

The behaviour of fission products once they have left the graphite surfaces are investigated in a third system of measurements. The bulk gas activity is monitored for gases, iodines and metals in the fast probe and the plate-out probe, and the data is used to determine the plate-out behaviour and eventually the decontamination factors for iodine and metals.

Because the migration of strontium and caesium to the coolant is long compared to the average fuel element dwell times, a proposal by Dr. Freck of the Central Electricity Generating Board to pre-load a graphite fuel tube with strontium-85 and to monitor this gamma-activity in the probe is being pursued, and is planned to be inserted in September 1970. Finally, the influence of corrosion on the evaporation of strontium-85 from pre-loaded graphite will be carried out in the centre spike injection facility.

CONCLUSIONS

The methods of assessing the behaviour of fission products and the corrosion in an HTR which will form part of the safety arguments used by the Design and Construction Companies have been described. The fission product and chemistry programmes for the Dragon Reactor have been planned to prove these methods as rapidly as possible in conditions which simulate a power reactor as closely as possible.

Present data has been applied to a notional reactor core to indicate the available margins. These are adequate except for the continuous release of iodine from fuel which fails during irradiation; proof of back-up safeguards in a helium system is underway. The corrosion margins both in 'normal' operation ('normal' inleakage is an elusive quantity) and in accident conditions also appear adequate using present information.

The present cycle of prediction and measurement in the Reactor Experiment will provide a major part of the confidence in these methods.

ACKNOWLEDGMENT

The confidence in the safety of an HTR stems from the work of the entire Project. In the interpretation of the safety aspects made in this lecture, particular acknowledgment is due to the Dragon Operations Group, the Materials Division and the team at Harwell led by Dr. Flowers. I would like to thank, in particular, Dr. Rowland,

Messrs. W. Browning and D. Kinsey for their valuable contributions and Mr. R. Gray and Mrs. Macey for their efforts in preparing this lecture.

Table 4. Core Chemistry

Helium pressure:	52 atm
Helium inventory:	3.0 tonnes (circulating)
H ₂ /H ₂ O ratio:	4.0
H ₂ O concentration:	30 μ atm
Tolerable continuous water inleakage:	600 g/day
Clean-up plant flow:	450 g/s
Peak hot-spot corrosion:	78 mg/cm ²

Fig. 1. Fission Product Release Estimation.

Fig. 2. Route for Assessment of Circuit Chemistry And Core Corrosion.

Fig. 3. Peak Systematic Fuel and Graphite Surface Temperatures for the Peak Channel of the Reference Core.

Fig. 4. Estimated Proportion of Exposed Fuel During Service (Peak Channel Hot-Spot).

Fig. 5. Effective Uniform Failed Particle Fraction Throughout Life.

Fig. 6. Effective Uniform Failed Particle Fraction Throughout Life.

Fig. 7. Effective Uniform Failed Particle Fraction Throughout Life.

Fig. 8. Safety Programme of Dragon.

THE RELATION OF FISSION PRODUCT RELEASE LIMITATIONS
TO THE DESIGN AND OPERATION OF A LARGE H.T.R. STATION

R.H. Flowers

099 0000

ABSTRACT

The key features concerning fission product release from a power HTR are:-

- (a) Release of ^{131}I during normal operation due to helium leakage.
- (b) Plate-out of ^{131}I and ^{137}Cs on primary circuit surfaces.
- (c) Release of ^{90}Sr during a reactor depressurisation, possibly following a leakage of steam into the circuit.

By considering the various diffusion and evaporation processes involved in the transport of these particular fission products from their points of origin it is possible to relate the fuel element design, and in particular the coated particle breakage fraction, to the severity of those features.

The most important parameters required for this calculation are:-

- (a) diffusion coefficients for I_2 , Cs and Sr in fuel tube graphites, as functions of temperature, concentration and impurity content,
- (b) adsorption isotherms for I_2 , Cs and Sr on fuel tube graphites, as functions of the same variables,
- (c) steady state R/B values for short-lived gases diffusing from broken particles,
- (d) plate-out factors describing the distribution of released I and Sr between the helium coolant and the primary circuit surfaces to which access may be required for maintenance purposes,
- (e) 'lift-off' factors describing the extent to which Sr and I_2 adsorbed on graphite or steel

surfaces will be transferred to the coolant during a steam ingress or depressurisation accident.

Of these parameters (a), (b) and (c) are known with 'order of magnitude' accuracy, and the available data together with some recent Harwell measurements are summarised. By means of the finite difference code, FIPDIG3, the best estimates of these data are used to compute the releases of ^{131}I , ^{137}Cs and ^{90}Sr into the coolant of an HTR over its whole life, and to assess the consequences of overheated channel accidents.

Parameters (d) and (e) are uncertain to the extent of at least two orders of magnitude, and for the purpose of these calculations it has been necessary to take somewhat arbitrary values for them.

Having arrived at values for plated-out and helium-borne activity corresponding to a certain broken particle fraction, deductions are made concerning the permissible breakage fraction in a 3000 MW(T) station.

It is apparent from this type of calculation that attainment of the necessary cleanliness of primary circuit and environment requires high standards of particle integrity. In view of the cost penalty associated with manufacture and quality control at very high standards it is considered worthwhile to improve our knowledge of the critical parameters listed above in order to reduce the uncertainty in our estimate of permissible breakage fraction.

In addition to laboratory measurements of parameters (a), (b) and (c) it is proposed that a reactor loop be constructed at Harwell for the purpose of measuring (d) and (e) directly under realistic conditions, and to provide direct checks of FIPDIG3 conclusions.

The proposed loop, known as Pluto B, will comprise a nearly isothermal fuel section, a cross flow boiler of realistic design and a helium circulation system which produces heat fluxes of the right order in both fuel element and boiler. By means of this equipment it will be possible to obtain plate-out factors, plate-out distribution profiles and lift off factors which will be very much more convincing than our present estimates of those quantities.

1. INTRODUCTION

Fission products constitute a formidable problem in harnessing of nuclear fission to power production, and the solutions of this problem represent a very significant part of the high capital cost of nuclear power stations. In the current generation of power reactors the fission products are confined within sealed metallic cans, and although many of them are volatile substances the reliability of the cans is high enough to reduce the fission product release to negligible proportions. The engineering problem resolves into one of rapid detection and replacement of leaking cans, combined with very high standards of can manufacture.

A new situation has arisen with the HTR. Although the primary fission product containment is still a sealed can, in this case a combination of PyC and SiC coating shells, the failure of a can leads less directly to contamination of the coolant circuit. This is because a barrier of graphite is placed between the coated particle and the coolant. Consequently, there is a time delay, varying from one fission product to another, between can failure and release of radioactive material.

Another consequence of the HTR type of fuel canning is that can failure is more predictable than before. A power reactor will contain about 10^{10} coated particles, so that there is a very good chance that manufacturing defects will result in a reproducible constant failure fraction. Warning of failure due to abnormal reactor operation must also occur in much smaller increments and in a more progressive manner than in a conventional reactor.

It is interesting to observe that, rather than reduce the emphasis on fission product behaviour, the benefits referred to have caused a large expansion in the quantity of information demanded by designers and safety assessors. There are two main reasons for this. Firstly the non-metallic fuel can is regarded as reaching a lower standard of leak tightness than metallic cans, although this is not necessarily a valid generalisation, and secondly designers are predictably anxious

to take full advantage of the new-found second line of defence in fission product retention, by pushing particle temperatures to their limit and by taking credit for retention in the graphite fuel tube. The possibility of omitting the SiC coating from particles is an example of the latter approach, because the fuel tube then becomes the primary, and a very efficient, barrier to Sr release.

Consequently, a detailed investigation of fission product behaviour is in progress. It covers the various diffusion and evaporation processes by which nuclides could move from the fuel kernels to the outside atmosphere or to the steam-raising machinery, and it seeks to infer the acceptable standards of manufacture and operation of the fuel.

2. A SUMMARY OF THE PROBABLE FISSION PRODUCT RELEASE RESTRICTIONS

The first step in attempting an estimate of tolerable coated particle damage is to decide which fission products and which modes of release are likely to be limiting.

A sufficiently accurate decision may be made, in the first instance, by comparing an 'index of hazard', given by the product of the ICRP recommended ingestion limit, the typical reactor inventory and the release potential for every fission product in turn. This has been done for the case of an HTR, operating on a 1500 day fuel dwell time with a 0.5 cm thick fuel free zone, using the function $\text{erfc} \frac{x}{2\sqrt{Dt}}$ as a rough measure of release potential. D is the diffusion constant of the fission product in nuclear graphite, x is the fuel free zone thickness and t is the dwell time. A calculation of this kind leads to the conclusion that the nuclides listed in Table 1, in order of decreasing hazard index, constitute the major part of the fission product release problem.

Using the computer code ICON⁽¹⁾ the inventory of these nuclides in a 3000 MW(T) reactor, at an equilibrium state of refuelling and with an initial loading of 33 tonnes of 5% enriched uranium, has been calculated for inclusion in Table 1.

Table 1. Significant Fission Products

Nuclide	Inventory (Curies) of a 3000 MW HTR at Refuelling Equilibrium
¹³¹ Iodine	8.3×10^7
¹³³ Iodine	1.6×10^8
⁸⁸ Krypton	6.6×10^7
⁹⁰ Strontium	5.5×10^6
¹²⁹ Tellurium(m)	6.2×10^6
¹³² Tellurium	1.2×10^8
¹³⁴ Caesium	4.4×10^5
¹³² Iodine	1.2×10^8
¹³⁷ Caesium	7.4×10^6
¹³⁴ Iodine	2.0×10^8
⁸⁷ Krypton	5.5×10^7

The problem of γ radiation from primary circuit components, although not directly related to this hazard index, is largely due to ¹³⁴Cs, ¹³⁷Cs and ¹³¹I, and so both aspects are covered by a consideration of the nuclides in Table 1.

It is convenient to group together those nuclides which show similar diffusion characteristics in HTR fuel, and to represent each group by its most hazardous member. In the following discussion we will therefore be concerned only with ¹³¹I, ⁸⁸Kr, ⁹⁰Sr and ¹³⁷Cs, and with the gaseous precursors ⁹⁰Kr and ¹³⁷Xe.

In making estimates of the quantities of these fission products which will diffuse through and evaporate from the fuel tubes in a large HTR it is useful to have a guide to the maximum permissible releases of each nuclide, so that a minimum coated-particle performance may be specified. The provision of such a guide is made difficult by the fact that, to some extent, deficiencies in coated particles may be off-set by such expedients as iodine filters in the reactor vent system or local

shielding of maintenance areas, and only a cost optimisation can resolve such flexibilities. Nevertheless a preliminary calculation of maximum permissible releases, based upon a clear set of reasonable assumptions, serves as a convenient illustration in this paper.

The following assumptions are made for a twin 1500 MW(T) station, of 30 years life:-

- (1) The maximum permissible level of external radiation to the public is 0.5 rem/year (whole body), as recommended by the ICRP⁽²⁾.
- (2) The maximum permissible annual dose limit for inhalation and ingestion by the public is one tenth of the ICRP recommendation. It is convenient to express that recommendation in terms of 'Derived Working Limits', which give the corresponding milk contamination levels for I, Sr and Cs since milk consumption represents the greatest ingestion risk (Table 2a).
- (3) Dose rates from primary circuit components should not exceed 200 mR/hour at points which may require even very infrequent access, one week after shutdown.
- (4) The helium leakage is on average 0.2% per day.
- (5) The station boundary fence is at 500 metres.
- (6) Leaks are discharged from a stack of 10 metres effective height, through a filter giving a factor of ten decontamination for Cs and Sr, but not for I₂.
- (7) Plate-out of I₂, Cs and Sr, is uniform over the boiler surfaces, the total boiler volume being $5 \times 10^8 \text{ cm}^3$.
- (8) The plate-out factor for these elements is 1000 under normal operation or depressurisation, and 100 during a boiler tube failure or channel overheating.
- (9) The maximum possible primary circuit breach has a diameter of 20 cm.

Table 2. Assumed Tolerable Release Limits

(a) Continuous Release

Derived Working Limits for Radioactivity in Milk.

Nuclide	Annual Average Activity
^{131}I	400 pC/litre
^{90}Sr	800 pC/g Ca
^{137}Cs	30,000 pC/litre

(b) Accidental Release

Derived Emergency Reference Levels of Cloud Dosage, based on inhalation hazard.

Nuclide	D.E.R.L. (Air)
^{131}I	0.03 C.sec/m ³
^{137}Cs	0.93 C.sec/m ³
^{90}Sr	0.00083 C.sec/m ³

- (10) Failure of a single boiler tube will not cause pressure relief valves to lift.
- (11) Inhalation and whole body exposure limits for accident situations are the Emergency Reference Levels recommended by the Medical Research Council⁽²⁾. These are Levels below which countermeasures such as evacuation, or banning of foodstuffs, are unlikely to be necessary. The significant limits in this group are

^{131}I	25 rad to the thyroid
^{90}Sr	1.5 rad/year to bone
^{88}Kr	15 rad whole body dose
^{137}Cs	10 rad whole body dose

Ingestion is not counted a limited risk in accident situations since doses from that source may be avoided by temporary banning of foodstuffs. The Emergency Reference Levels have been expressed as 'Derived Emergency Release Levels' in Table 2b.

On the basis of these assumptions, potential hazards from normal operation, and from certain accident conditions, have been assessed, and a maximum permissible release from the core calculated in respect of each case. The most limiting cases for each isotope are summarised in Table 3.

Single channel overheating and boiler tube fracture accidents do not result in limits as severe as those in Table 3.

It must be stressed that these are merely illustrative calculations, and that large uncertainties remain in some of the assumptions made. In the case of normal ^{131}I release the main uncertainty is probably the plate-out factor, and the same applies to ^{90}Sr release during depressurisation. The uncertainties associated with ^{131}I and ^{137}Cs γ radiation in the circuit are the distribution of plate-out, the extent to which design can alleviate maintenance difficulties, and the presence of activated impurities alongside the fission products.

Table 3. Derived Maximum Permissible Core Releases into the Circuit for each of the 1500 MW(T) Reactors

Nuclide	Derived Limit	Context
^{131}I	$R/B = 2 \times 10^{-5}$	Leakage in normal operation
^{88}Kr	$R/B = 4 \times 10^{-3}$	Leakage in normal operation
^{137}Cs	5 Curies/year	Circuit contamination
^{131}I	$R/B = 10^{-5}$	Circuit contamination
^{90}Sr	36 Curies/year	Depressurisation accident without lift-off
^{88}Kr	$R/B = 3 \times 10^{-3}$	Depressurisation accident without lift-off

3. PARAMETERS WHICH CONTROL FISSION PRODUCE RELEASE FROM PARTICLES INTO THE REACTOR COOLANT

The sources of fission products in any configuration of fuel matrix, graphite and coolant will be the effective proportion of exposed kernels and the fractional uranium contamination of the outer pyrolytic carbon coatings. In order to assess the release of a given fission product into the coolant during a given fuel element operation, it is necessary to evaluate the influence of a number of release barriers for the chosen set of circumstances.

The barriers which are known to operate, and which will be considered, are:-

- (a) diffusion from particles having imperfect coatings,
- (b) diffusion from recoil sites resulting from uranium contamination of the particle outer coatings,
- (c) diffusion through the fuel matrix,

- (d) diffusion through the graphite wall of the fuel pin,
- (e) evaporation from the outside of the graphite wall into the helium coolant.

Some additional fission product delay could occur in the gap between matrix and fuel tube. It is not possible to take this into account at present, since experiments show that the majority of transmission of metals occurs at the inevitable, but ill-defined, contact points.

In order to make use of models to calculate the overall effect on products of these barriers, the following parameters are required for each significant fission product:-

- (a) Diffusion constant in the bare kernel, as a function of burn-up and temperature, or empirical data on fractional release from bare kernels as a function of those variables.
- (b) Diffusion constant from recoil sites in the outer pyrocarbon layer of particles as a function of temperature and concentration, or empirical data on fractional release from intact particles of known contamination level.
- (c) Diffusion constant in the matrix material as a function of temperature and concentration, in respect of both 'through diffusion' and diffusion from recoil sites.
- (d) Diffusion constant in the fuel tube graphite as a function of temperature and concentration. It is necessary to consider the other fission products, and impurity levels, when determining concentration.
- (e) Adsorption isotherm describing the equilibrium partition of the fission product between graphite surface and the adjacent helium boundary layer, as a function of temperature. Like (d) this requires a consideration of other species present, and extent of oxidation of the surface.
- (f) Diffusion constant in the helium boundary layer as a function of temperature. A knowledge of the boundary layer thickness, based on helium Reynolds Number is also necessary.

A knowledge of these parameters, taken in conjunction with an estimated temperature-time history and the geometrical constants of a fuel element, allows the use of computer codes^(3,4) to calculate the release as a function of element life. Apart from the margin of uncertainty on these parameters, the remaining factors which affect the accuracy of the calculations are uncertainties about the extent of particle deterioration in service and about the extent of Sr and Cs removal from the fuel tube outer surfaces due to corrosion by coolant impurities. Such phenomena must be considered in addition to the diffusion and evaporation steps evaluated here.

4. PRESENT STATE OF KNOWLEDGE OF KEY PARAMETERS

4.1 Release from Particles having Imperfect Coatings has been measured in irradiation experiments, but the actual number and severity of the imperfections has been determined only approximately during post-irradiation metallography. For this reason, in the following parametric survey of fission product release from fuel elements, the coated particles are characterized by attributing to them a certain R/B value for each nuclide, assuming that it remains constant during irradiation. Further irradiation, using uncoated kernels, would be required to relate these values to actual numbers of completely broken coatings; however, due to the wide spectrum of severity of imperfections, it is probably preferable to specify batches of fuel according to their R/B $\left(= \frac{\text{release rate}}{\text{birth rate}} \right)$ for ^{133}Xe , ^{85}Kr and ^{89}Sr , after a few days at temperature. In that way, the detailed mechanics of coating failure are avoided, and the available data are sufficient to allow the derivation of fractional release for other significant nuclides from results on these three representative species.

The experimental data on medium and short-lived Kr and Xe isotopes shows that R/B varies as $1/\sqrt{\lambda}$, and that $(\text{R/B})^{133}\text{Xe}$ for bare kernels is between 10% and 30% at 1400°C (Pluto V and VI)⁽⁵⁾. More recently Bildstein⁽⁶⁾ has shown, by Xe pressure measurements, that 70%-80% of stable rare gases were released into the accessible voidage of Dragon UO_2 coated particles at around 1500°C . The R/B for a Kr or Xe isotope leaving coated particles will depend upon the effective broken particle

fraction, ϕ , the decay constant, the diffusion constant and the effective grain radius 'a', i.e.

$$\frac{R}{B} = \phi \cdot \frac{3}{A} \left(\coth A - \frac{1}{A} \right) \quad \text{where } A = \sqrt{\frac{\lambda a^2}{D}} \quad (1)$$

Zumwalt⁽⁷⁾ has shown that D_{Xe} in UC_2 crystals is given by the equation:

$$\log_{10} D = -5.34 - \frac{68000}{2.3 RT}$$

which gives $D_{Xe}(1250^\circ C) = 8 \times 10^{-16} \text{ cm}^2/\text{sec.}$

If we set $(D/a^2)_{Xe}$ at $1250^\circ C$ equal to 10^{-8} , in order to correspond to releases at least as large as those seen in Pluto V and VI, a value of crystallite radius 'a' equal to 2.8μ is derived.

An empirical value of D/a^2 should be obtained for a particular type of kernel, at a particular burn-up, by observation of R/B for at least one rare gas isotope, but it is perhaps useful to give, as in Table 4, the equilibrium $(R/B)_{1250^\circ C}$ values which correspond to a 2.8μ effective radius crystallite with $\phi = 1$. In estimating R/B for krypton isotopes it is assumed that $D_{Kr} \approx 4 \times D_{Xe}$, as is indicated by various coated particle irradiation results at O.R.N.L. and Dragon. The measurement of R/B for a long-lived nuclide, such as ^{85}Kr or ^{137}Cs , is required for the estimation of ϕ .

The value of establishing an empirical D/a^2 for a particular batch of fuel at a particular burn-up lies in the fact that the 'a' parameter, representing the effective crystallite radius, is likely to be sensitive to manufacturing route and burn-up. The activation energy is less likely to be affected.

Experimental results on release of iodine and metallic fission products from bare kernels are again very sparse. In Pluto Loops V and VI, where essentially bare kernels were irradiated, the ^{131}I and ^{137}Cs results were low due to hold up of these isotopes in the matrix graphite.

Nevertheless fractional releases of a few per cent were recorded. The fact that UC_2 kernels were used is no disadvantage, since the kernels of faulty particles will be reduced to UC_2 in the reactor. On theoretical grounds, we would expect ^{131}I to show a diffusion behaviour in UC_2 rather similar to ^{133}Xe , and the measured $D_{\text{Ba}}^{(7)}$ of $5 \times 10^{-15} \text{ cm}^2/\text{sec}$ at 1250°C suggests 100% release of all significant Cs, Sr and Ba isotopes from 2.8μ radius UC_2 crystallites. It therefore appears that no retention of Cs can be assumed for particles with imperfections in all coating layers, or of Sr and Ba for particles with imperfections in the SiC layer.

With these assumptions about the behaviour of imperfect particles, the releases from actual assemblies of particles may be predicted from measurements of ^{133}Xe , ^{85}Kr and ^{89}Sr release, as indicators of the two different types of imperfection. Thus rare gas releases may be derived from $(\text{R/B})^{133}\text{Xe}$ using Table 4 as a guide, ^{131}I and ^{137}Cs releases may be equated to $(\text{R/B})^{133}\text{Xe}$ and $(\text{R/B})^{85}\text{Kr}$ respectively, and ^{140}Ba and ^{90}Sr releases may be equated to $(\text{R/B})^{89}\text{Sr}$.

4.2 Release from Uranium Contamination in Particle Coatings

On the basis of a 2.8μ effective crystallite radius in kernels ($\text{R/B}^{133}\text{Xe}$ 0.25 at 1250°C), the fractional release of ^{131}I and rare gases due to Uranium contamination of pyrocarbon, is less than that due to an equal fraction of exposed kernels. For this reason release from uranium contaminant is unimportant unless it exceeds the quantity in exposed fuel.

Information on release from recoil sites in pyrocarbon comes from two sources; fractional releases measured after loose particle irradiations in which no imperfect particles were present, and laboratory measurements of diffusion constants.

Most batches of particles used in DRAGON irradiations since around 1967 have been sampled and α counted. A correlation between α count and actual amount of uranium present in the outer pyrolytic carbon layer has been demonstrated.

Table 4. (R/B) Rare Gas from Bare Kernels of 2.8μ
Effective Crystallite Radius at 1250°C

Species	R/B	Species	R/B
^{128}Xe	1	^{85}Kr	1
^{133}Xe	2.5×10^{-1}	^{87}Kr	4.8×10^{-2}
^{135}Xe	6.5×10^{-2}	^{88}Kr	7.2×10^{-2}
^{137}Xe	5.4×10^{-3}	^{89}Kr	9.9×10^{-3}
^{138}Xe	1.0×10^{-2}	^{90}Kr	4.1×10^{-3}
^{140}Xe	1.3×10^{-3}	^{91}Kr	2.1×10^{-3}
^{141}Xe	4.7×10^{-4}		

In Table 5 the releases of some rare gases and metals observed in three experiments with loose particles at about 1250°C maximum are shown. In the absence of any bursts of gas, or metallographic evidence of coating damage, we conclude that these are releases from recoil sites in the outer pyrocarbon. The evidence to date thus suggests that release of the metals is complete, but that the rare gases, particularly the short-lived isotopes, are greatly attenuated. This result is in agreement with what is known of diffusion constants in pyrolytic carbon.

For matrix fuel the recoil sites are likely to be somewhat different from those in loose particle experiments, but no data are available on present matrix materials.

4.3 Diffusion through the Matrix Graphite

The physical properties of the matrix are as yet insufficiently finalised to warrant any very precise calculations on its permeability to gases and metals. In view of the fact that the delaying effect of the matrix is less important than that of the fuel tube, because it is a distributed source, it will be sufficiently accurate to assume that the

Table 5. Releases Attributed to Coating Contamination at 1250°C

Dragon Experiment	Fractional U-Contamination	Fractional Release ($\pm 50\%$)					
		^{133}Xe	^{135}Xe	^{137}Cs	^{140}Ba	^{89}Sr	^{90}Sr
LEHPD 2	6×10^{-6}	4×10^{-7}	10^{-8}	-	-	-	-
Studsvik 16R	6×10^{-6}	10^{-7}	5×10^{-8}	-	-	-	-
HTE (Rod 3)	$< 4 \times 10^{-5}$	-	-	5×10^{-5}	10^{-5}	2×10^{-5}	2×10^{-5}

matrix behaves exactly like the fuel tube graphite. Table 6 shows on this basis how little effect upon release of metals is expected from a typical matrix at 1200°C.

4.4 Diffusion Constants for Fission Products in Fuel Tube Graphite

4.4.1 Strontium There is a strong concentration and temperature dependence of D , and the available data is insufficient to allow a suitable function to be fitted accurately over wide ranges of these variables. At the present time we have chosen to divide the measurements into two groups, following the example of Besenbruch⁽⁸⁾, and to construct Arrhenius type equations representing the regions above and below 1 mg/g.

Above 1 mg/g the work of Faircloth⁽⁹⁾, Bromley⁽¹⁰⁾, Zumwalt⁽¹¹⁾ and Riedinger⁽¹²⁾ gives, very approximately, a line as follows:-

$$\log_{10} D = -1.90 - \frac{4.99 \times 10^3}{T} \quad (D \text{ in cm}^2/\text{sec}) \quad (2)$$

Table 6. Fractional Releases from a Graphite Matrix

Nuclide	D_{matrix} at 1200°C (approx. $\text{cm}^2\text{sec}^{-1}$)	D/a^2 ($a = 0.5 \text{ cm}$)	F (equil.)	Time to reach F/2 (days)
^{140}Ba	$7 \cdot 10^{-9}$	$3 \cdot 10^{-8}$	0.5	13
^{89}Sr	$7 \cdot 10^{-9}$	$3 \cdot 10^{-8}$	0.9	30
^{90}Sr	$7 \cdot 10^{-9}$	$3 \cdot 10^{-8}$	1	30
^{137}Cs	$2 \cdot 10^{-7}$	$8 \cdot 10^{-7}$	1	1

The data were obtained on a variety of graphites, such as PGA, EY9 and HX30, and in general range over a ten-fold variation in D at a given temperature. Below 1 mg/g we have diffusion constants derived from profiles in fuel tubes⁽¹³⁻¹⁷⁾, some out-of-pile results from GGA^(11,12,18), and some recent work by Sandalls⁽¹⁹⁾. In view of the uncertainties of temperature and fuel failure rate in the profile measurements, and of concentration levels in the GGA data, we have used Sandalls' data, which is well represented by the equation:-

$$\log_{10} D = 1.164 - \frac{11200}{T} \quad (3)$$

This work was done on AGL9 Gilsocarbon graphite and was reproducible within a factor of two in D . The fact that coefficients derived from the profile measurements are in general a factor of ten lower than Sandalls' results suggests that the concentration dependence of D requires further examination, especially with a view to confirming behaviour at the full burn-up concentration ($R/B = 10^{-3}$) of around 1 $\mu\text{g/g}$. The graphite characteristics, such as surface

area and impurity content, may also be significant, and in particular it should be noted that the AGL 9 graphite contains 30 ppm of calcium.

For the purpose of running FIPDIG 3 at this time, the following functional representation of D was used, taking into account the total concentration of all strontium and barium isotopes:-

$$\log_{10} D = \left(1.164 - \frac{11200}{T} \right) + \left[\left(\frac{6210}{T} - 3.064 \right) \cdot \frac{e^{4.4 \times 10^{-3}(C - 10^3)}}{1 + e^{4.4 \times 10^{-3}(C - 10^3)}} \right] \quad (4)$$

where C = concentration in $\mu\text{g/g}$

D in cm^2/sec

T in $^{\circ}\text{K}$

This equation connects the two Arrhenius functions by a fairly steep step at 1 mg/g loading. In Fig. 1 are shown all of the available measurements for D_{Sr} , together with the two Arrhenius lines (A and B) comprising eq. 4.

4.4.2 Caesium As with Sr, there is a strong temperature and concentration dependence. There are data at tracer concentration (1 $\mu\text{g/g}$) from the work of Bryant⁽²⁰⁾, and from fuel tube profiles^(21,17) which give approximately a line

$$\log_{10} D = 0.19 - \frac{10390}{T} \quad (5)$$

In the region 100-1000 $\mu\text{g/g}$ we have data⁽¹⁹⁾ on AGL 9 graphite, giving a line:-

$$\log_{10} D = -1.158 - \frac{3940}{T} \quad (6)$$

which lies close to points obtained on other graphites by Bromley⁽¹⁰⁾. Riedinger⁽²²⁾ gives two results lying midway between these groups.

Again there is no information on how these regions connect, but the Cs adsorption isotherms do show some evidence of a change in site energy at around 30 $\mu\text{g/g}$ ⁽²⁰⁾. We have therefore inserted a step function at that point, to give finally:-

$$\log_{10} D = \left(0.19 - \frac{10390}{T}\right) + \left[\frac{\left(\frac{6450}{T} - 1.348\right) \cdot e^{0.147(C - 30)}}{1 + e^{0.147(C - 30)}} \right] \quad (7)$$

where C = concentration in $\mu\text{g/g}$

D in cm^2/sec

T in $^{\circ}\text{K}$

Fig. 2 shows all the available points for D_{Cs} together with the two lines (A and B) corresponding to equ. 5 and 6.

4.4.3 Iodine There appear to be no diffusion data for iodine at relevant temperatures, but work at lower temperatures indicates that at high concentrations it diffuses like a gas⁽²³⁾. We, therefore, treated it like N_2 gas⁽²⁴⁾ and introduced a pressure and temperature dependence assuming that gas-gas collisions in pores control the diffusion and that collision cross-sections are energy independent.

Longstaff gives an apparent D of $3 \times 10^{-3} \text{ cm}^2/\text{sec}$ at 1 atm and room temperature, for a non-impregnated graphite with no total pressure gradient. Assuming a connected porosity of 8% we have

$$D_{\text{I}_2} \text{ (at 55 atm)} = \frac{3 \times 10^{-3} \times T^{3/2}}{55 \times (293)^{3/2} \times 0.08} \quad (8)$$

$$\text{i.e. } D = 1.32 \times 10^{-7} \times T^{3/2} \text{ cm}^2/\text{sec}$$

At this level of D there is no significant delay of ^{131}I in 5 nm of fuel tube above 800°C .

4.4.4 Rare Gases There are no data at realistic temperatures and pressures but in this case we can be more confident of the extrapolation formulae. The expression used was exactly as for I_2 ⁽²⁴⁾.

In summary it may be said that the currently available data on diffusion of metals and iodine in fuel tube graphite is of order of magnitude accuracy only. Features which are under examination, with a view to improving this situation, are

- (a) The effect of concentrations on D.
- (b) The occurrence of multiple diffusion processes.
- (c) The effect of helium impurities on D.
- (d) The differences between the various possible fuel tube graphites.

4.5 Adsorption Isotherms for Fission Products on Fuel Tube Graphite

4.5.1 The GGA data on Sr adsorption recently improved by the use of tungsten effusion cells⁽¹⁸⁾, give the following Freundlich isotherm for TS - 688 graphite:-

$$\ln P = \left(a + b \frac{10^3}{T} \right) + \left(c + d \frac{10^3}{T} \right) \cdot \ln C \quad (9)$$

with $P = (\text{atm})$

$C = \mu \text{ mole/g}$

$a = 10.27$

$b = - 38.1$

$c = - 0.745$

$d = 4.13$

The equation contains a correction to allow for the change in surface area caused by grinding the graphite to $37\text{--}74 \mu$ particle size; it is considered to represent the experimental points to

within a factor of five. Recent results from Harwell confirm these data⁽²⁵⁾ within a factor of 3.

In view of the fact that these measurements do not extend to the required level of 1 $\mu\text{g/g}$ there is some considerable risk in extrapolating as a Freundlich isotherm; therefore, at a concentration of 30 $\mu\text{g/g}$ the equation has been changed to a Langmuir isotherm for extrapolation purposes. This may be a realistic approximation because some reactor graphites contain around 30 $\mu\text{g/g}$ of calcium, which would be expected to have the effect of producing an apparent Langmuir isotherm. The final equation used is:-

$$P = \exp \left[\left(a + b \frac{10^3}{T} \right) + \left(c + d \frac{10^3}{T} \right) \ln C \right] + \exp \left(\ln C + 15.17 - \frac{47000}{T} \right) \quad (10)$$

C = concentration (μ mole/g)

The heat of adsorption in the Langmuir region has been set at 94 k.cal/mole, in line with the low concentration value measured at GGA.

4.5.2 For Cs Adsorption we use the data of Milstead⁽²⁶⁾ on TS - 668 graphite. The following approximate equation was fitted to his results:-

$$P = \exp \left[22.6 - \frac{60450}{T} + \ln C \right] + \exp \left[11.6 - \frac{63500}{T} + 4.48 \ln C \right] \quad (11)$$

where P = (atm)

C = concentration ($\mu\text{g/g}$)

In this case the experimental points do suggest a change of slope at 30 $\mu\text{g/g}$. The isopiestic technique used for this work involved lump specimens of graphite, so a surface area correction is unnecessary.

4.5.3 Salzano⁽²⁷⁾ gives data for adsorption of I_2 on National Carbon type TSX graphite, in the temperature range 100-800°C.

The concentrations used are quite comparable to those expected in fuel tubes. Salzano's isotherms are very dependent upon outgassing temperature, and in particular the effect of H_2O corrosion was to lower pressures by up to a factor of ten. We might expect the isotherm to depend also upon impurity levels, but in the absence of other information the formula

$$P = \frac{C^2 \sqrt{T}}{7.8 \times 10^{-5} \times \exp\left(\frac{20000}{T}\right)} \quad (12)$$

where $P = (\text{atm})$

$C = \text{concentration } (\mu\text{g/g})$

$T = {}^\circ\text{K}$

was used. It applies to graphite outgassed at 900°C .

In summary it is clear that the metal adsorption isotherms are inadequately defined in the significant concentration region around $1 \mu\text{g/g}$. The possible effect of impurities has yet to be investigated.

5. METHOD OF CALCULATION OF CORE RELEASE

The one dimensional time dependent fission product diffusion code FIPDIG has been fully described by Preinreich⁽³⁾. The programme can be used to calculate fission product concentration profiles and release rates for any arrangement of fuel zones and fuel-free zones in slab, cylindrical or spherical geometry. The source term and zone temperatures may be varied with time. In its original version the code used diffusion and evaporation constants which were independent of time, and therefore of concentration, although provision was made for ascribing different values to each of the temperature zones. The evaporation constants used in this early work were not related to the respective adsorption isotherms, but were best estimates from the inadequate experimental data available.

A modified form of FIPDIG, known as FIPDIG 3, is at present in use at Harwell⁽²⁵⁾. This code incorporates the temperature and concentration

dependent functions for the diffusion and evaporation constants within the original FIPDIG framework, and therefore takes into account automatically the changes in D and H . The evaporation constant H is derived from the equilibrium partial pressure of the fission product at the evaporating surface by means of an approximation of the type:-

$$H = \frac{D}{d} 0.023 (\text{Re})^{0.8} (\text{Sc})^{0.3} \frac{f(c)}{C} \text{ cm sec}^{-1} \quad (13)$$

where: D = diffusion coefficient of evaporating species in boundary layer

d = hydraulic diameter of coolant channel (taken as 0.5 cm)

Re = Reynolds Number of coolant (taken as 40,000)

Sc = Schmidt Number of coolant

c = concentration of evaporating species in surface of fuel tube

$f(c)$ = gas phase concentration of that species in equilibrium with the surface.

The value of D is obtained for this purpose from a simple kinetic theory correlation:

$$D = \frac{3}{8 \sigma^2 n} \left(\frac{RT(M_1 + M_2)}{2\pi M_1 M_2} \right)^{\frac{1}{2}} \quad (14)$$

where σ = mean molecular diameter

n = number of gas molecules per cc

M_1, M_2 = molecular weights of helium and the fission product respectively.

In FIPDIG 3 the values of D and H appropriate to a particular time-step are obtained by inserting a concentration term, relating to the sum of all stable isotopes of the element, into equations such as 3, 5, 8 and 9. The time-step is then computed a second time, using

these values in conjunction with the yield and half-life data of the particular isotope under examination. In the case of ^{89}Sr and ^{90}Sr the yields of stable barium have also been added into the first step, since it may be expected that Sr and Ba diffuse at similar rates.

By means of FIPDIG 3 a time dependent fission product release may be computed for regions of a power reactor core, the regions being chosen so that there is little variation of temperature or power history within each one. A summation of such results gives the overall release from the core for a particular management scheme.

Rather than present a calculation for a single special case, the following sections are an attempt to use FIPDIG 3 to illustrate the magnitude of core release of the key fission products resulting from a selection of operating temperatures. Temperature is the most important single variable after broken particle fraction.

6. PARAMETRIC SURVEY

An externally cooled fuel pin, with the following dimensions will be evaluated:-

Fuel Matrix I.D.	26 mm
Fuel Matrix O.D.	42 mm
Graphite Sleeve I.D.	42 mm
Graphite Sleeve O.D. (a)	50 mm
(b)	52 mm
Power/unit length (constant)	0.5 kw/cm

The variables chosen were:

Equivalent broken particle fraction, denoted by ϕ :	10^{-3} and 10^{-5}
Graphite sleeve thickness:	4 and 5 mm
Species:	Cs 137, Sr 90, I 131 and rare gases

The temperatures were chosen as representative of the range of interest, with the graphite sleeve thickness divided into two parts to simulate the temperature gradient. Power, temperature and ϕ were assumed independent of time.

Case	T _{Fuel}	T _{inner sleeve}	T _{outer sleeve}	
A	1400	1100	1000	°C
B	1200	925	850	
C	1000	840	800	

This survey represents 12 calculations for each nuclide. In addition, single runs were performed to check the effect of the different half-life in the case of ^{89}Sr .

In the cases of the metallic fission products the fission product source term is equal to the equivalent broken particle fraction, but for ^{131}I and the short-lived rare gases there is a significant delay and decay in the kernels of broken particles. In order to include that effect in the parametric survey we define the 'equivalent broken particle fraction', ϕ , as being equal to the steady state R/B of ^{137}Cs and ^{85}Kr from the particles, and furthermore we assume that the equivalent grain radius of the kernel, 'a', is such as to give R/B = 0.25 for ^{133}Xe release from a broken particle at 1250°C. Thus at 1250°C, an equivalent broken particle fraction of 10^{-4} would result in R/B = 10^{-4} for the ^{137}Cs , R/B = 2.5×10^{-5} for ^{133}Xe and ^{131}I , and smaller R/B values for the short-lived gases. A further parameter, the equivalent SiC damage fraction ϕ' , should also be stated, to cover the instance when SiC damage exceeds significantly the complete coating damage. ϕ' is defined as the steady state R/B of ^{89}Sr or ^{90}Sr from an assembly of particles and should be used in place of ϕ when predicting releases of metals other than Cs.

In the present exercise it is assumed for convenience that $\phi = \phi'$, implying that there are no particles with defective SiC and intact PyC.

6.1 Discussion of Results on Cs and Sr

The results of the survey are shown in Figs. 3-6.

The dominant effect is temperature, due to the large enthalpy of evaporation at the cooled surface and to the large activation enthalpy of the diffusion processes. 150° in fuel sleeve temperature is worth about two orders of magnitude in integral fractional release at the 2000 day point, and considerably more at shorter times.

The integral fractional release of ^{137}Cs or ^{90}Sr is seen to rise sharply with time and to level off eventually at the value of ϕ . At the highest temperature considered here (curve A), the fractional release comes close to ϕ at 2000 days, whereas curves B and C are far below. Figs. 7-9 show a sequence of profiles of ^{137}Cs concentration at successive times, for the three temperature conditions. On the basis of the data used it appears that at long irradiation times the large benefits obtainable by lowering outer fuel tube temperatures result more from the fall in the H value than from the lower D. ^{90}Sr curves are similar.

The survey shows the fractional release to be directly proportional to the ϕ value. This is because the metal concentrations involved are less than $1\text{ }\mu\text{g/g}$, making the diffusion coefficients independent of concentration and giving a Langmuir adsorption isotherm for the evaporation step. The use of much larger ϕ values might alter this picture, as the Freundlich adsorption regime is entered and the D begins to rise, but on the basis of the present assumptions this would require practically 100% broken particles. The effect of changing ϕ as irradiation proceeds, to simulate particle damage, may therefore be estimated from Figs. 3-6 by direct summation of the differing particle histories.

The time dependences of the ^{137}Cs and ^{90}Sr results are very similar. This may be considered somewhat unrealistic on the grounds that at mg/g loadings Cs is known to diffuse faster than Sr, but as explained earlier the available data at $\mu\text{g/g}$ loadings are insufficient to support any more refined assumptions.

The effect of a change from 4 mm to 5 mm in fuel sleeve thickness is seen to be greater at the lower temperatures and early in the element life. It is less important than fuel sleeve temperature in determining metal release.

^{89}Sr must be released to a lesser extent than ^{90}Sr , due to decay during passage through the fuel sleeve. As an example, one point on the parametric survey (Case B) was repeated using ^{89}Sr decay constant. Table 7 shows the result.

For this reason the hazard from ^{89}Sr evaporating as metal can be neglected in comparison to the ^{90}Sr .

Table 7. Ratio of ^{89}Sr to ^{90}Sr Fractional Release

Time (days)	Integral Fractional Release ($\phi = 10^{-3}$, 0.5 cm fuel tube)		$^{90}\text{Sr}/^{89}\text{Sr}$
	^{89}Sr	^{90}Sr	
600	1.3×10^{-7}	2.0×10^{-6}	15
1000	1.3×10^{-7}	4.9×10^{-6}	38
2000	1.3×10^{-7}	1.3×10^{-5}	100

6.2 Discussion of Results on ^{131}I

The FIPDIG 3 programme, using the data on iodine discussed earlier, showed that at graphite temperatures above 800°C there is negligible delay in the fuel sleeve. The fractional release of ^{131}I therefore quickly reaches the value pertaining to the particles themselves. Fig. 10 shows this result for a selection of temperature and (R/B) particle combinations. Under these assumptions the controlling factor is the (R/B)_{particles}, which depends on the ϕ and D/a^2 parameters discussed earlier.

If we take "equivalent failed particle" fractions, ϕ , equal to 10^{-3} and 10^{-5} , assume an equivalent grain radius α equal to 2.8μ and assume also that $D_{Xe} = D_I$, then the results shown in Table 8 are obtained covering the parametric survey conditions.

The temperature dependence of ^{131}I release is thus rather less than that found for the metallic fission products, and the steady state R/B is controlled by the particle kernel temperature rather than the fuel sleeve temperature. The sleeve thickness is on this basis unimportant, and there is a linear dependence upon the equivalent broken particle fraction.

Table 8. R/B for ^{131}I under the Parametric Survey Conditions

Fuel Temp. (°C)	$D_I (\text{cm}^2/\text{sec})^{(7)}$	$(R/B)_{\text{particles}}$ (= fractional release from element)	
		$\phi = 10^{-3}$	$\phi = 10^{-5}$
1000	9.1×10^{-18}	3.2×10^{-5}	3.2×10^{-7}
1200	3.6×10^{-16}	2.0×10^{-4}	2.0×10^{-6}
1400	6.0×10^{-15}	8.4×10^{-4}	8.4×10^{-6}

6.3 Inert Gas Releases

The rare gases, like iodine, are controlled largely by diffusion within the kernels of broken particles, but the shortest half-life members suffer some decay also during their passage through the fuel sleeve wall. Since the time to reach a steady state R/B is in most cases controlled by the half-life rather than the diffusion coefficient, these times are always short compared with the fuel element lifetime. Consequently no FIPDIG cases were run on gas release; steady state R/B values, for the fuel element conditions covered in our parametric survey,

were computed from $D_{\text{Kr,Xe}}$ in UC_2 kernels⁽⁷⁾ and in reactor grade graphite (equ. 8). As for I_2 , we have assumed a 2.8μ 'effective grain radius' a . Since the release is proportional to ϕ , the equivalent breakage fraction, results are given only for $\phi = 10^{-3}$. A fuel sleeve thickness of 5 mm is taken since the result is rather insensitive to a change from 5 to 4 mm.

The depletion of rare gases diffusing through the fuel sleeve wall is given by⁽²⁸⁾ :-

$$\frac{F_o}{F_i} = e^{-\lambda T}$$

where F_o = current leaving

F_i = current entering

λ = decay constant

$T = \frac{X}{\sqrt{\lambda D}} - \frac{0.693}{\lambda} = \text{apparent delay time}$

X = graphite thickness

D = diffusion coefficient (equ. 8)

This equation assumes a constant current of gas entering the inside face and a zero concentration at the outside face.

Table 9 shows how, for 5 mm of graphite at 1000°C , the sleeve depletion factor varies with diffusion coefficient and decay constant.

In Table 10 the $(R/B)_{\text{element}}$ values are given, with the $(R/B)_{\text{particles}}$, for the parametric survey cases.

This table serves to illustrate the temperature and half-life dependence of $(R/B)_{\text{element}}$, although in an absolute sense it holds only for the particular values of ϕ and 'a' assumed.

Table 9. Depletion Factor for Rare Gas Diffusion
through 0.5 cm Graphite

Isotope	Decay Constant sec ⁻¹		Depletion Factors (1000 ⁰ C)		
			$D = 1 \times 10^{-3}$	6×10^{-3}	$3 \times 10^{-2} \text{ cm}^2 \text{ sec}^{-1}$
Xe 133	1.52	- 6	1	1	1
135	2.11	- 5	1	1	1
137	2.96	- 3	0.99	1	1
138	6.79	- 4	1	1	1
139	1.69	- 2	0.30	0.86	1
140	4.33	- 2	0.094	0.52	1
141	4.08	- 1	0.0002	0.03	0.32
Kr 85m	4.41	- 5	1	1	1
87	1.48	- 4	1	1	1
88	6.95	- 5	1	1	1
89	3.63	- 3	0.84	1	1
90	2.10	- 2	0.25	0.78	1
91	7.08	- 2	0.043	0.36	1
92	2.31	- 1	0.002	0.09	0.5

Table 10. Calculation of the R/B Values for Rare Gases Escaping from a Fuel Element
under the Conditions chosen for the Parametric Survey

Equivalent Breakage Fraction (ϕ) = 10^{-3} Fuel Sleeve Thickness = 0.5 cm Equivalent UC_2 Grain Radius (a) = 2.8μ

Nuclide	Fuel 1400°C Sleeve (inner) 1100°C Sleeve (outer) 1000°C		Fuel 1200°C Sleeve (inner) 925°C Sleeve (outer) 850°C		Fuel 1000°C Sleeve (inner) 840°C Sleeve (outer) 800°C	
	(R/B) _{particles}	(R/B) _{element}	(R/B) _{particles}	(R/B) _{element}	(R/B) _{particles}	(R/B) _{element}
¹³³ Xe	6.8×10^{-4}	6.8×10^{-4}	1.7×10^{-4}	1.7×10^{-4}	2.6×10^{-5}	2.6×10^{-5}
¹³⁵ Xe	1.7×10^{-4}	1.7×10^{-4}	4.4×10^{-5}	4.4×10^{-5}	6.8×10^{-6}	6.8×10^{-6}
¹³⁷ Xe	1.5×10^{-5}	1.5×10^{-5}	3.7×10^{-6}	3.7×10^{-6}	5.7×10^{-7}	5.7×10^{-7}
¹³⁸ Xe	2.8×10^{-5}	2.8×10^{-5}	7.1×10^{-6}	7.1×10^{-6}	1.0×10^{-6}	1.0×10^{-6}
¹⁴⁰ Xe	3.6×10^{-6}	1.8×10^{-6}	9.2×10^{-7}	4.8×10^{-7}	1.4×10^{-7}	7.2×10^{-8}
¹⁴¹ Xe	1.3×10^{-6}	3.9×10^{-8}	3.2×10^{-7}	9.6×10^{-9}	4.9×10^{-8}	1.5×10^{-9}
⁸⁵ Kr	10^{-3}	10^{-3}	10^{-3}	10^{-3}	10^{-3}	10^{-3}
⁸⁷ Kr	1.3×10^{-4}	1.3×10^{-4}	3.4×10^{-5}	3.4×10^{-5}	5.0×10^{-6}	5.0×10^{-6}
⁸⁸ Kr	2.0×10^{-4}	2.0×10^{-4}	4.9×10^{-5}	4.9×10^{-5}	7.5×10^{-6}	7.5×10^{-6}
⁸⁹ Kr	2.7×10^{-5}	2.7×10^{-5}	7.1×10^{-6}	7.1×10^{-6}	1.0×10^{-6}	1.0×10^{-6}
⁹⁰ Kr	1.1×10^{-5}	8.3×10^{-6}	2.7×10^{-6}	2.0×10^{-6}	4.3×10^{-7}	3.2×10^{-7}
⁹¹ Kr	5.7×10^{-6}	1.9×10^{-6}	1.4×10^{-6}	4.7×10^{-7}	2.2×10^{-7}	7.3×10^{-8}

The two rare gases of greatest interest are ^{90}Kr and ^{137}Xe , on account of their long-lived daughter products. The release of these gases into the primary circuit causes a corresponding fractional release of ^{90}Sr and ^{137}Cs to be deposited uniformly. It follows that early in the life of an element, these metals arise largely from the gas releases, whereas later on the direct metal evaporation will probably predominate. To illustrate this point the appropriate $(R/B)_{\text{element}}$ values from Table 10 have been plotted in Figs. 4 and 6, from which it is clear that at lower temperatures the gas contribution predominates for a longer period.

7. SPECIFICATION OF PERMISSIBLE 'EQUIVALENT BROKEN PARTICLE FRACTION'

The results given in the previous section relate to hypothetical core regions of constant power and temperature. By making a number of reasonable assumptions we can estimate from them the approximate behaviour of reactor cores in which the specified temperatures relate to the hottest point of the peak power channel. Let us consider a 1500 MW(T) core with a 1500 day mean fuel dwell time.

7.1 ^{90}Sr and ^{137}Cs

Let A_{1500} = no. of Curies released in 1500 days per 0.5 kW of element length (from the parametric survey)

Now assume:-

- (a) A random reloading pattern.
- (b) No variation of fuel tube temperature along the core radius or as a function of time.
- (c) The fractional release from a channel is always one fifth of the fractional release at the hottest point, due to the axial temperature profile.

Then the Curies/year released from the core at refuelling equilibrium is given by

$$A_{1500} \times \frac{365}{1500} \times \frac{1500 \times 10^3}{0.5} \times \frac{1}{5} \quad (15)$$

Table 11. Estimated Core Release Rates of Metals

($\phi = 10^{-3}$ and fuel tube thickness = 0.5 cm)

Parametric Survey Case (related to hottest fuel tube position)	Curies/year from a 1500 MW(T) core	
	^{90}Sr	^{137}Cs
A	210	240
B	2.9	2.4
C	0.15	0.053

Table 11 shows the result of such an estimate, using only the data obtained with an 'equivalent failed fraction' of 10^{-3} and a 0.5 cm thick fuel tube.

7.2 ^{131}I and Significant Rare Gases

In deriving R/B for iodine and gases from the parametric survey results (Tables 8 and 10) it is necessary to apply a correction for the spectrum of coated particle temperatures in the core. A 100°C drop in particle temperature gives approximately a factor of two reduction in R/B, on the basis of the data used in the survey. We have allowed a factor of two in R/B, both axially and radially, as compared to R/B at the hottest fuel tube position of the peak power channel.

Table 12 gives the resulting values of (R/B) core.

If we now convert the rare gas R/B estimates to (Curies/year) of the daughter products in a 1500 MW(T) reactor, we obtain the results given in Table 13, which must be added to those of Table 11 to obtain overall Cs and Sr releases.

Table 12. Estimated Core R/B Values for Non-Metals

Parametric Survey Case (related to hottest fuel tube in peak channel)	$(R/B)_{\text{core}} \phi = 10^{-3}$			
	^{131}I	^{90}Kr	^{137}Xe	^{88}Kr
A	2.1×10^{-4}	2.1×10^{-6}	3.8×10^{-6}	5.0×10^{-5}
B	5.0×10^{-5}	5.0×10^{-7}	9.3×10^{-7}	1.2×10^{-5}
C	8.0×10^{-6}	8.0×10^{-8}	1.4×10^{-7}	2.0×10^{-6}

Table 13. Estimates of Metals Released as Precursors

 $(\phi = 10^{-3})$

Parametric Survey Case	Curies/year from a 1500 MW(T) core	
	^{90}Sr	^{137}Cs
A	3.5	6.7
B	0.83	1.7
C	0.13	0.25

8. COMPARISON OF ESTIMATED AND PERMITTED RELEASES

By comparing the estimates of fission product release at $\phi = 10^{-3}$ with the permissible releases derived in Section 2, we can infer the values of ϕ which would be permissible, on the basis of the assumptions made, in a reactor with peak conditions corresponding to the parametric survey temperatures.

In Fig. 11 this comparison is made, and in Table 14 the derived values of permissible ϕ are given.

Table 14. Permissible Values of 'Equivalent Broken Particle Fraction' Corresponding to the Particular Assumptions Made

Nuclide	Limiting Context	ϕ permitted		
		A	B	C
^{131}I	Normal leakage	10^{-4}	5×10^{-4}	3×10^{-3}
^{88}Kr	Normal leakage	8×10^{-2}	0.3	1
^{137}Cs	Circuit contamination	2×10^{-5}	10^{-3}	1.7×10^{-2}
^{131}I	Circuit contamination	5×10^{-5}	2×10^{-4}	10^{-3}
^{90}Sr	Depressurisation	2×10^{-4}	10^{-2}	0.13
^{88}Kr	Depressurisation	6×10^{-2}	0.25	1

Case	$T_{\text{fuel}} (^{\circ}\text{C})$	$T_{\text{sleeve}} (^{\circ}\text{C})$
A	1400	1000-1100
B	1200	850-925
C	1000	800-840

Thus at the highest set of temperatures (Case A) the plate-out of ^{137}Cs is the limiting factor; it requires $\phi < 2 \times 10^{-5}$. At the lowest set (Case C) the plate-out of ^{131}I is limiting, requiring $\phi < 10^{-3}$. Particularly striking is the rapid increase of metal evaporation with increasing outer fuel tube temperature. With only 50°C difference between A and B, there is a factor of 50 in the consequent limits on ϕ .

In order to relax the limits, taking Case B as a fairly realistic example of peak channel conditions, the first step would be to increase the circuit radiation level tolerance above the assumed 200 mr/hr. The second step, of similar importance, would be to increase the height of the ventilation stack, or to show that I_2 plate-out is greater than $10^3:1$, or to install a form of iodine filter in the stack. By these means it appears that the limiting ϕ value could be raised to, say, 10^{-3} if the economic factors demanded it.

It is interesting to note that the tolerable fraction of failed SiC coats is higher than the tolerable fraction of complete coating failures. Thus, using the Sr release as a criterion of the necessary integrity of SiC coats, the permitted value of ϕ' is as high as 10^{-2} in case B.

Another factor which may further relax the SiC integrity requirement, as determined by the depressurisation accident, is the possible raising of the ^{90}Sr D.E.R.L. from 0.00083 to 0.09 Curie.sec per cubic metre.⁽²⁹⁾ Such a change would, on present assumptions, remove the necessity of SiC coatings, but considerable improvement in accuracy of key data is required before that possibility could become a real option.

9. DISCUSSION OF FUTURE DATA REQUIREMENTS

There is at least a factor of ten uncertainty in computed releases of Sr and Cs, due to uncertainties in the diffusion and adsorption constants of these metals in graphite as functions of temperature and concentration. A particularly important task is that of deciding the shape of the adsorption isotherms below $10\ \mu\text{g/g}$ loadings, and of investigating the effect of impurities such as calcium on them. Work

is in progress on these problems both at GGA and at Harwell.

Another area requiring further experimentation is the measurement of the release rates of iodine and rare gas isotopes from assemblies of uncoated or damaged fuel particles. This information is important in establishing the validity of the suggested procedure of predicting iodine and short-lived gas R/B values from measurements of ^{133}Xe release. Experiments of this kind should also be used to discover the extent to which the effective crystallite size in kernels can be increased in order to decrease the steady state releases of those fission products.

Assuming that all these measurements are made satisfactorily, we may expect computations of the FIPDIG 3 type to predict accurately the core releases of significant fission products. However, it will be necessary to check the predictions against realistic simulations of typical HTR core conditions.

The extent and the distribution of fission product plate-out within the core and primary circuit is almost as important as the core release; it is in this area that our estimates of hazards from normal helium leakage and from γ activity of components requiring maintenance, may be in error by up to two orders of magnitude. Unfortunately the prediction of plate-out factors and plate-out profiles is a peculiarly difficult task, because adsorption isotherms for the fission products on metal surfaces are sensitive to the surface condition, and the mass transfer coefficients of fission products in a cross flow steam boiler are dependent upon particle size in an almost unpredictable manner.

In view of the cost penalty associated with manufacture and quality control of coated particles to very high standards, and with guarantees of very low failure rate in service, it is essential to have accurate information on the gas-borne and plated-out activity resulting from a known fraction of failed particles.

10. PLUTO LOOP B

It is intended that a reactor loop, known as Pluto B, be constructed at Harwell during the next two years for the purpose of checking FIPDIG 3 predictions and illustrating the plate-out to be expected in a large HTR.

By means of this equipment it will be possible to obtain plate-out factors, distribution profiles and accident lift-off factors which will be very much more convincing than our present estimates of those quantities.

The loop will comprise a ten inch fuel pin with very small axial temperature gradient, a cross-flow boiler of realistic design and a helium circulation system which will produce heat fluxes of the right order in both fuel element and boiler surfaces.

Fig. 12 shows a schematic circuit of Pluto B. The fuel pin is made to operate like the hottest pin in a reactor channel by heating the helium electrically to simulate the other 90% of the fuel length. The helium flow rate and pressure is comparable to that required in a similarly sized channel of a power reactor. Downstream of the fuel, a full length vertical section of a cross-flow boiler is simulated by means of a water-cooled unit as shown in Fig. 13.⁽³⁰⁾ Heat fluxes, and therefore metal surface temperatures, are controllable over a range of values appropriate to the different parts of a steam boiler. A gas bearing circulator returns the helium to the electrical heater at 300°C. Table 15 summarises some key parameters in the loop design.

The first task of Pluto B will be to present a picture of the gradual build-up of ^{137}Cs , ^{134}Cs , ^{90}Sr and ^{131}I on the boiler surfaces, using a fuel pin containing a known fraction of uncoated fuel particles. Continuous measurement of helium-borne activity and of chemical impurities will be made. The deposition surfaces will be particularly easy to examine, since individual tubes may be removed and replaced from outside the pressure vessel.

Other tasks will be simulation of depressurisation accidents, to measure any desorbed or dislodged material, channel overheating accidents, to measure increased evaporation from the fuel, and steam ingress accidents, to measure changes in helium-borne activity.

In summary the Pluto B loop will serve to check the assumptions made in our hazard assessment and parametric survey of fission product release.

Table 15. Design Parameters for Pluto Loop B

Parameter	Pluto Loop B Design Value
Normal Operation	
(a) Gas Temperature $^{\circ}\text{C}$ Outlet from Circulator	300
Inlet to fuel	840 Design maximum
Outlet from fuel	900 Design maximum
Inlet to Heat Exchanger	880 Design maximum
Outlet from Heat Exchanger	300 Design maximum
(b) Gas Pressure psi (abs)	1000
(c) Gas Flow Rate (g/sec)	150
(d) Fuel Element O.D. cm	6.5 (max.)
Channel Diameter cm	7.0 (max.)
Fuel Rating Kw/cm	Up to 1.6
Pin Length cm	25
Pin Power Kw	Up to 40
(e) Electrical Heating Power kW	400
(f) Clean-up By-pass Flow %	0.7
(g) Boiler or deposition section	
(i) Reheater: He temp. $^{\circ}\text{C}$	750-690 Design Value
Average heat flux watts/cm ²	33.0
Tube diameter cm	2.54
Lateral/longitudinal pitch cms	As required
Tube material	As required
(ii) Superheater: He temp. $^{\circ}\text{C}$	690-550
Average heat flux watts/cm ²	26.0
Tube dia. and pitching	As reheater
Tube material	As required
(iii) Evaporator: He temp. $^{\circ}\text{C}$	550-460
Average heat flux watts/cm ²	20
Tube diameter and pitching	As reheater
Tube material	As required
(iv) Economiser: He temp. $^{\circ}\text{C}$	460-300
Average heat flux watts/cm ²	15.0
Tube diameter and pitching	As reheater
Tube material	As required

11. CONCLUSIONS

(a) The most important fission product hazards in the HTR are ^{131}I , ^{133}I , ^{137}Cs , ^{134}Cs , ^{90}Sr , ^{88}Kr , ^{90}Kr and ^{137}Xe .

(b) Release of Cs and Sr, while basically controlled by the fraction of broken particle coatings, is strongly influenced by fuel tube surface temperature. The latter temperature should be given some consideration together with the particle temperature when setting design limits.

(c) Release of I_2 and rare gases is controlled by the fraction of broken particle coatings and by the particle temperature. Gaseous precursors may be the major source of metal fission product release during early fuel pin life, particularly at the lower temperatures.

(d) Data on diffusion, adsorption and plate-out of fission products is at present insufficiently accurate and complete to allow a confident assessment of allowable particle breakage fraction. However, on the basis of reasonable assumptions, allowable fractions have been estimated for a variety of operating temperatures.

(e) A new in-pile loop, to be built at Harwell, is described. Its function will be to confirm or correct assumptions and calculations of the type presented here.

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Fig. 1 Strontium diffusion in reactor graphite.

Fig. 2 Caesium diffusion in reactor graphite.

Fig. 3 Integral fractional release of ^{137}Cs when $\phi = 10^{-5}$.

Fig. 4 Integral fractional release of ^{137}Cs when $\phi = 10^{-3}$.

Fig. 5 Integral fractional release of ^{90}Sr when $\phi = 10^{-5}$.

Fig. 6 Integral fractional release of ^{90}Sr when $\phi = 10^{-3}$.

Fig. 7 Time dependent concentration profiles for ^{137}Cs when $\phi = 10^{-3}$ - Case A.

Fig. 8 Time dependent concentration profiles for ^{137}Cs when $\phi = 10^{-3}$ - Case B.

Fig. 10 R/B for ^{131}I leaving the fuel tube.

Fig. 9 Time dependent concentration profiles for ^{137}Cs when $\phi = 10^{-3}$ - Case C.

Fig. 11 Comparison of permitted and estimated fission product releases.

Fig. 12 Schematic circuit of Pluto B loop.

Fig. 13 The deposition unit of Pluto B loop.

DISCUSSION

T. A. Jaeger: I would like to hear some more details about the finite difference code FIPDIG3, and I would be interested to hear whether you have also considered utilizing for these calculations the finite element method which; e.g. has been successfully applied to heat diffusion problems in complicated geometries.

R. H. Flowers: The FIPDIG3 code calculates concentration of a fission product along a fuel element radius at any number of specified times after start up. It is possible to vary fission rate, number of broken fuel particles and zone temperatures throughout the course of the calculation, and the diffusion and absorption coefficients are computed at each time step from appropriate functions of temperature and concentration. The "finite element" method has not, as yet been applied to the problem, but a two dimensional version of FIPDIG3 may eventually be used to replace the manual axial integration described by Mr. Ashworth.

S. I. Kaplan: Do you intend to conduct dust deposition experiments in your loop (Pluto B)?

R. H. Flowers: Yes, the object of using an expensive crossflow deposition unit is to obtain realistic plate-out profiles for fission products carried on particles of greater than molecular size. The molecular size. The molecular species could be equally well studied in a tubular deposition tube. We intend to run the loop under sets of well-defined oxygen potentials and water ingress rates in order to produce dust of representative size distribution and quantity. Of course, if laboratory measurements of dust samples, taken from prototype reactors, become available it might be valuable to inject a similar mixture into the loop to find out the plate-out pattern on a crossflow steam boiler.

D. D. Tytgat: To what extent will the irradiation conditions in the Pluto B loop be comparable to the HTR power reactor?

R. H. Flowers: The loop will irradiate a short length of a realistic fuel element at the chosen temperature, with helium cooling under the correct load condition. The helium will be cooled from around 750° C.

to 300° C. in a crossflow water-cooled deposition unit, the tube surface temperatures and heat fluxes being adjustable to match the desired steam boiler conditions very closely in all important respects.

L. R. Zumwalt: 1. Do you have any evidence for or utilize a "fast" diffusion coefficient for the diffusion of metals (Cs and Sr) in graphite, 2. How do you estimate the evaporation coefficient for metal fission products from graphite as used in your calculations?

R. H. Flowers: We have seen long "tails" on diffusion profiles obtained from inpile experiments and these might be the result of rare gas precursor diffusion. However, some recent out-of-pile isotopic diffusion experiments at Harwell, using strontium in the concentration range 0 - 100 mg/g, have yielded unambiguous profiles corresponding to a single diffusion rate. We are hopeful that the earlier results were anomalous "precursor" or concentration effects, but it is too early to be definite on that point. Evaporation coefficients are obtained by assuming that the equilibrium metal vapor pressure exists at the graphite surface. A conventional mass transfer equation of the type $kd/D = .023 Re^{0.8} Sc^{0.3}$ is then used to give the evaporation coefficient.

J. H. Norman: Would you compare fission product release for a reactor with tubular elements and with telephone dial elements for similar power outputs?

R. H. Flowers: In surveys of fission product release from cores comprising elements of different geometries, it is always found that the fuel tube surface temperature controls the Cs and Sr emission, while the coated particle temperature (and, therefore, similar iodine and rare gas release) showed a greater metal release from the tubular element. The tubular element had a somewhat higher linear power rating, however.

FISSION PRODUCT TRANSPORT IN HTGR SYSTEMS - A SUMMARY*

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ABSTRACT

A brief summary of fission product transport in a gas-cooled reactor is presented. Analytical methods applicable to reactor situations are presented along with a variety of old and new experimental data to illustrate the calculational techniques. These methods indicate that the gaseous activity in a well-designed gas-cooled reactor system can be easily held to acceptable levels and will consist almost entirely of noble gases. Some plateout activity will result from these noble gas precursors as well as from the halogen precursors which are found to have release rates essentially equal to noble gases. The other fission products, provided normal precautions are taken, will not be released from the fuel elements in sufficient quantities to significantly increase the plateout levels above that resulting from these gaseous precursors.

INTRODUCTION

The intent of this paper is (1) to point out the main features of fission product behavior in HTGR systems, (2) to summarize recent developments in the analysis of fission product release and plateout, and (3) to summarize the results of recent investigations of fission product release.

A typical HTGR system might have fuel consisting of uranium and thorium dicarbide kernels (microspheres) overcoated with layers of pyrolytic carbon and dispersed in a bonding or filler matrix of carbonaceous

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material. Oxide kernels, as well as dicarbide kernels, can be used; also, silicon carbide layers are often incorporated in the fuel particle coatings. The fuel material is contained in fuel elements consisting of graphite blocks which are cooled by a circulating helium coolant stream.

The primary barrier to the release of fission products into the helium coolant system is the coating on each individual fuel particle. An effective secondary barrier for metallic fission products is the retention and sorption property of the fuel matrix material and the fuel element structural graphite. These barriers limit the release of fission products into the helium coolant stream. Gas-borne fission products that are released into the coolant are maintained at a satisfactory level by employing a bypass cleanup system. This helium purification system continuously processes a portion of the coolant stream through a series of adsorption beds culminating in a cryogenic bed which removes essentially all of the fission products in this helium stream.

In addition to the helium purification system, the steam generators and other reactor components are effective in removing (by plateout) fission products other than the noble gases. Consequently, the noble gases constitute the majority of the gaseous activity in a reactor circuit.

DIFFUSION OF FISSION PRODUCTS

The transport of fission products in a stagnant medium is generally described by a diffusion equation of the form

$$\frac{\partial C}{\partial t} = \nabla \cdot (D \nabla C) - \lambda C + B, \quad (1)$$

where C is the concentration, t is the time, D is the diffusion coefficient, λ is the decay constant, and B is the birth rate per unit volume. In coated fuel particles with spherical symmetry and a constant diffusion coefficient, this equation takes the form

$$\frac{\partial C}{\partial t} = \frac{D}{r^2} \frac{\partial}{\partial r} \left[r^2 \frac{\partial C}{\partial r} \right] - \lambda C + B. \quad (2)$$

This equation can be used to treat the formation, diffusion, and decay of a fission product nuclide in either the kernel or coating, or both. Generally, a buffer layer of low-density pyrolytic carbon exists between kernel and coating, but this can be neglected.

For the case where the birth rate is constant throughout a particle (e.g., a bare uranium carbide particle), the solution to Eq. (2), assuming time-independent properties and zero initial and outside concentrations, is readily obtained. The release of fission products is usually expressed in terms of the R/B value, which is the instantaneous release rate divided by the birth rate. This R/B value as a function of time is given by

$$\begin{aligned} \frac{R}{B}(t) &= - \frac{D 4 \pi r^2}{\frac{4}{3} \pi r^3 B} \left. \frac{\partial C}{\partial r} \right|_{r=a} \\ &= 3 \left(\frac{D}{\lambda a^2} \right)^{\frac{1}{2}} \left[\coth \left(\frac{\lambda a^2}{D} \right)^{\frac{1}{2}} - \left(\frac{D}{\lambda a^2} \right)^{\frac{1}{2}} \right] - 6e^{-\lambda t} \sum_{n=1}^{\infty} \frac{\exp(-n^2 \pi^2 D t / a^2)}{n^2 \pi^2 + \lambda a^2 / D}, \end{aligned} \quad (3)$$

where a is the outside radius of the particle. This expression starts at zero at time zero and reduces to the first term as t approaches infinity. That is,

$$\frac{R}{B}(\infty) = 3 \left(\frac{D}{\lambda a^2} \right)^{\frac{1}{2}} \left[\coth \left(\frac{\lambda a^2}{D} \right)^{\frac{1}{2}} - \left(\frac{D}{\lambda a^2} \right)^{\frac{1}{2}} \right]. \quad (4)$$

This equation represents the steady-state behavior. The solid line on Fig. 1 is a plot of Eq. (4) and illustrates that the release from uncoated (bare) particles should be proportional to the square root of the fission product half-life for R/B values less than 0.1.

If there is a pyrolytic carbon coating on the particle, Eq. (2) must be solved with a step change in the birth rate at the kernel/coating interface--down to a value indicative of coating contamination. The dashed lines on Fig. 1 illustrate the behavior that would be expected for different values of the birth rate ratio B_2/B_1 , where B_2 and B_1 are the birth rates in the coating and the kernel, respectively. It should be noted that the behavior is quite different if there is no contamination in

the outer coating; i.e., $B_2/B_1 = 0$. However, it should also be noted that the position of the curve for $B_2/B_1 = 0$ depends on the ratio of the kernel and coating diffusion coefficients, the sorptive abilities, and the radii of the two regions. (The equilibrium R/B value for coated particles is discussed in detail by Dunlap and Gulden.¹⁾

NOBLE GAS RELEASE

Numerous experiments on noble gas fission product release from coated particles have been conducted. The results of some typical experiments that were carried out by Gulf General Atomic (GGA) are shown in Fig. 2 in plots of the steady-state Kr-85m R/B values vs operating time for the GA2-284-6F2 fuel capsule (Cell I),² the GAIL IIIB loop,³ the GAIL IV loop,⁴ and the Peach Bottom reactor.⁵ The data for the 6F2 capsule and the GAIL IIIB and GAIL IV loops are all at roughly comparable temperatures. However, the Peach Bottom reactor results should be multiplied by approximately a factor of 2 to account for a somewhat lower temperature than for the capsule and the GAIL tests. The increase in the R/B values with time is probably indicative of coating failure. The coated particles used in the 6F2 capsule, in GAIL IIIB, and in the Peach Bottom reactor (Core I) had an early type of coating and showed a relatively high degree of failure, whereas the coated particles used in GAIL IV had a later, improved type of coating and showed a low degree of failure.

Figures 3 and 4 are plots of the end-of-life R/B data vs half-life of the fission products. The data generally show a $(t_1)^{\frac{1}{2}}$ (square root of half-life) dependence and the lines were drawn with this slope. This half-life dependence indicates that the release is from either bare kernels or high contamination in the coatings. In no case was there an indication of the sharp increase in release of long-lived fission products in the manner indicated by the dashed curve with $B_2/B_1 = 0$ (see Fig. 1). (In all of these experiments the geometrical and operational complexities of the experiments should be borne in mind. Without going into details, it can be stated that a variety of difficulties usually affected the accuracy of the data points.)

To more fully understand the release process, the end-of-life Kr-85m R/B values at 1300°C for a number of ORNL, Dragon, and GGA experiments are assembled in roughly chronological order in Fig. 5. The first point is the result obtained by GGA⁶ for an uncoated carbide while the next two are the results obtained by ORNL for an uncoated carbide⁷ and a mechanically uncoated carbide.⁸ The next point is a Pluto IIIA result⁹ obtained by the Dragon Project. This result was multiplied by a factor of 5 to account for the 20% particle failure. The next six are early results on coated particles obtained from the GA 309 sweep capsule⁶ series irradiated in 1960-61. The next two are the results obtained from 1962 to 1964 for the GAIL IIIA and IIIB in-pile loop experiments.^{3,10} The next three are results from the three-compartment 6F2 sweep capsule² irradiated in 1963. The first compartment of this experiment had fuel coated with multiple layers of silicon carbide and pyrolytic carbon. Since only approximately one-fourth of the particles failed during the irradiation, the Kr-85m R/B value was arbitrarily multiplied by a factor of 4 to indicate release from the kernels alone.

The next point is from the GAIL IV experiment⁴ irradiated from 1964 to 1966. Since it was known to have a particle failure of only 0.8% at end-of-life, this value was arbitrarily multiplied by 125. The last point is from Peach Bottom Core I results.⁵ As mentioned previously, this result was increased by a factor of 2 to account for the lower temperatures. This temperature correction is obtained by assuming that the release rate has an exponential temperature dependence with an activation energy of approximately 30 kcal/g mole.⁶

It can be seen from Fig. 5 that all of the end-of-life Kr-85m R/B values are approximately the same if one makes corrections for the amount of failed particles. Therefore, it is likely that the end-of-life release is from exposed kernels rather than from diffusion out of contaminated coatings. Also, it was noted that particle damage ranged from minor distortion to gross distortion with the final kernel volume more than twice the original kernel volume. Therefore, it is likely that the characteristic dimension (a) in Eq. (3) is much smaller than the radius of the kernel.

The similarity of the releases from all of these experiments indicates that there is a noble gas release rate characteristic of failed fuel, and that the rate is relatively independent of initial material or of the degree of failure. Therefore, the reactor designer can establish the design noble gas release levels by determining the amount of fuel failure to be expected and using a release curve obtained from an appropriate experiment having approximately fully failed fuel. If one ignores precursor contributions, the number of noble gas atoms in the coolant stream is then governed by the equation

$$\frac{dn}{dt} = fB\left(\frac{R}{B}\right)_{\text{failed}} - n\lambda - nF, \quad (5)$$

where n is the total number in the coolant stream, f is the fraction of the fissions occurring in failed fuel, and F is the fractional removal rate by the purification system.

Although the situation is relatively simple from the reactor design point of view, the details of the noble gas release process need to be better understood. For example, there is not a clear understanding of the reason for the different krypton-to-xenon release ratios obtained from different experiments. The above-mentioned GGA experiments raise the possibility that this ratio is approximately 2-1/2 for badly failed fuel and 4-1/2 for relatively intact fuel. However, an ORNL experiment on uncoated carbide particles⁷ has indicated that there is no difference between the krypton and xenon release rates.

There is also disagreement among the various investigators about the interpretation of a half-life independent "recoil" release, as opposed to diffusive release. Vanslager has maintained that the word "recoil" does not adequately describe the release process, since the recoiling particle inevitably ends up back in a solid material from which it must be subsequently released. These problems, and many others, warrant further investigation.

HALOGEN RELEASE

The normal reactor operating temperatures are generally high enough so that the release of halogens from the fuel particles is essentially

identical to that of the noble gases. Figures 3 and 4 also include some of the iodine release results obtained from decay experiments and diffusion tube results. It can be seen that, within experimental accuracy, the release of iodine from the fuel particles is identical to that of xenon.

Further verification of the similarity of xenon and iodine release was obtained by recent work at ORNL. The release from pyrolytic carbon-coated uranium carbide particles with intentionally cracked coatings was investigated at a variety of temperatures. A series of tests was conducted by holding the cooling gas flow constant and raising the temperature in 200°C steps by increasing the flux. The Xe-133 and Xe-135 releases into the helium purge stream were measured at steady-state conditions and after a sudden capsule withdrawal, to determine iodine release. The continuing xenon release after withdrawal is from iodine decay. Figure 6, which is a typical plot of Xe-133 and Xe-135 decay at 1400°C, indicates that the release of iodine from failed particles is similar to that of xenon.

The released iodine is not adsorbed, or delayed to any significant extent, by the fuel element structural graphite. However, the metal surfaces of the reactor circuit are quite effective in removing iodine. The GAIL loop experiments have indicated that the majority of the iodine is removed in a single pass around the circuit. Computer programs have been developed¹¹ to handle this mass transport problem. The PAD code (Plateout Activity Distribution), under development at GGA, solves equations of the form

$$\begin{aligned} \frac{\partial N_j}{\partial t} &= S_{N_j} + \lambda_i N_i - N_j \left(\lambda_j + \frac{4k}{d} \right) - v \frac{\partial N_j}{\partial x} + \frac{4kK \sum M_j^p}{d(1 - \sum M_j/Ms_j)} \left(\frac{M_j}{\sum M_j} \right) \\ \frac{\partial M_j}{\partial t} &= S_{M_j} + \lambda_i M_i + kN_j - \lambda_j M_j - \frac{kK \sum M_j^p}{(1 - \sum M_j/Ms_j)} \left(\frac{M_j}{\sum M_j} \right), \end{aligned} \quad (6)$$

where

S_{N_j} , S_{M_j} = source terms for coolant concentration and surface concentration of fission product j ,

N_j, N_i = atom concentration of fission product j and its precursor i in the coolant stream,

M_j, M_i = atom surface concentration of fission product j and its precursor i on the plateout surface,

λ_j, λ_i = decay constant of fission product j and its precursor i ,

x, t = axial distance and time coordinates,

k = the mass transport coefficient,

d = the channel diameter,

V = the coolant flow velocity, and

$\frac{K \sum M_j^P}{(1 - \sum M_j / M_{s_j})} \left(\frac{M_j}{\sum M_j} \right)$ is an expression for the desorption of fission

product j from the surface into the coolant. The PAD code can handle an arbitrary number of species interacting through the summation term.

Figure 7 is a plot showing a typical calculated iodine distribution around a reactor circuit. It can be seen that the iodine concentration is greatly reduced by the reactor circuit components. For this case the loop removal rate is considerably greater than the purification system removal rate and, consequently, the total amount of loop plateout is adequately approximated by assuming instantaneous complete plateout in the circuit. The circuit distribution of iodine, however, can only be accurately estimated by employing a mass transport plateout code such as PAD.

ALKALI METAL RELEASE

The alkali metals rubidium and cesium are of intermediate volatility at normal reactor operating conditions. The short-lived isotopes are relatively unimportant, and their release can be bounded without serious consequences by using noble gas release rates from the fuel. However, Cs-134* and Cs-137 are of special interest because of their effect on maintenance operations.

*Cs-134 is formed by an (n, γ) reaction on fission product Cs-133.

The release of these metals from the fuel particles is likely described by diffusion equations such as Eq. (2). However, the long-lived species Cs-137 and Cs-134 will normally not reach equilibrium during the core lifetime. Consequently, the transient solution to the equation is required. Unfortunately, because of slow convergence of the series solution and added complications when considering a multiregioned fuel particle, the exact solution is not readily available. Therefore, a numerical solution to the equation is often attempted. The FIPER code,^{12,13} developed by GGA, is a numerical solution to the general diffusion Eq. (1). The code will treat any of the three basic one-dimensional geometries, i.e., slab geometry, radial cylindrical symmetry, or spherical symmetry.

Figure 8 shows some typical experimental Cs-137 data obtained from an out-of-pile isothermal anneal of an irradiated fuel particle. The solid line is the result of a FIPER analysis of the experiment using published diffusion coefficients¹⁴ for the particle coating. It can be seen that a reasonable approximation has been obtained. However, computer time limitations preclude performing a FIPER analysis of all of the fuel particles in a reactor core (each has a slightly different temperature and power history). Therefore, a closed-form approximation to the full numerical solution is required. Such an approximation is provided by the "release constant" model, in which the release rate from the fuel particles is assumed to be directly proportional to the inventory. A two-release-constant model presently under investigation by GGA is defined by the coupled equations

$$\begin{aligned}\frac{dn_1}{dt} &= B_1 - R_1 n_1 - \lambda n_1 \\ \frac{dn_2}{dt} &= B_2 + R_1 n_1 - R_2 n_2 - \lambda n_2,\end{aligned}\tag{7}$$

where n_1 and n_2 are the total inventories in regions 1 and 2 of the fuel particle, B_1 and B_2 are the respective birth rates, R_1 and R_2 are the respective release constants, and λ is the decay constant. (The single-release-constant model normally used in reactor calculations is, of course, defined by the first equation.) The dashed lines of Fig. 8 are the

two-release-constant approximation to the annealing-experiment data. It can be seen that these curves provide a reasonable approximation to the actual situation and can therefore be used for reactor calculations of the release from the fuel particles.

After the release from the fuel particles, there are sorption and diffusion in the fuel element materials and evaporation into the coolant stream. The complexity of the temperature/time history and the temperature dependence of the material properties again require a numerical solution to Eq. (1). The FIPER code, with a source given by the two-release-constant approximation to the release from the fuel particles, provides a reasonable treatment of the problem. In general, the transport process is complex and, under some circumstances, involves concentration-dependent diffusion coefficients and multiple diffusion processes. The overall transport process is highly temperature-dependent.

The loss from the fuel element surfaces is by evaporation described by Freundlich adsorption isotherms.¹⁵ At moderate temperatures the overall diffusion and evaporation process represents a major barrier to cesium loss from the core. Consequently, if the reactor temperatures are low enough, the direct loss will be negligible and the noble gas precursors will constitute the major source of Cs-137 activity. As an illustration of this retention ability, Figs. 9 and 10 present some cesium activity profiles measured on the Peach Bottom D06-01 fuel element and the GAIL IV fuel element.¹⁶ Each set of profiles is from a different elevation along the length of the fuel elements. It can be seen from these steeply sloping profiles that the direct loss of cesium in these experiments was negligible.

The curves in Figs. 9 and 10 are the results of an attempt to fit the data with the FIPER code. The cesium diffusion coefficient was the same for both of these experiments and, for some reason, appeared to be very small and to have the extremely low activation energy of 3 kcal/g mole. Over the temperature range investigated, the temperature dependence of the diffusion coefficient can actually be approximated by a temperature to the 3/2-power dependence, indicating that the rate-limiting step is possibly gas-phase transport at the low concentration levels of these experiments. (It should

be noted that these data cover three different types of graphite, two of which were impregnated.) These curves should be considered an illustration of the transient FIPER code results rather than a confirmation of a diffusion process; it is hoped that this difficulty can be resolved by examination of additional Peach Bottom elements.

The distribution of cesium activity around the coolant circuit is determined by mass transport calculations such as the PAD code. In general, it is mass-transport-limited rather than saturation-limited. Loop experiments¹⁷ have shown that an approximately exponentially decreasing distribution is obtained in isothermal regions with spikes at points of abrupt flow changes.

A silicon carbide coating is an extremely effective barrier for cesium as well as other metallic fission products.

ALKALINE EARTH RELEASE

The alkaline earth metals strontium and barium are only moderately volatile at normal reactor operating temperatures. Therefore, the release of isotopes other than Sr-89, Sr-90, and Ba-140 can usually be ignored. The 12.8-day Ba-140 chain is important for reactor maintenance considerations because of the high-energy gamma rays emitted by the La-140 daughter. The 30-year Sr-90 chain is important from the standpoint of reactor safety. This nuclide is a bone-seeking beta emitter which has a relatively high yield and long half-life.

The alkaline earth metals, being smaller in diameter than the alkali metals, show considerably more solubility in pyrolytic carbon and thus greater release from coated particles than that illustrated by the results of the Cs-137 anneal in Fig. 8. This is shown, for example, by the ORNL in-pile retention data given in Table 1. However, there is still significant holdup in the fuel material and in the graphite structural members. Silicon carbide coatings effectively retain the alkaline earth metals as indicated by ORNL data given in Table 1 and by GGA data given, for example, in Ref. 18.

Figure 11 shows the results of a series of out-of-pile strontium diffusion experiments performed at GGA. The solid lines on these plots are the results of a FIPER analysis using the GGA reference design diffusion coefficient data.* It can be seen that a reasonable fit to the data has been obtained, considering the experimental difficulties and uncertainties.

Barium, compared to strontium, has an even longer diffusional holdup; thus, 12.8-day Ba-140 will decay very significantly during transit through the structural graphite. The Xe-140 precursor will therefore constitute the major source of Ba-140 activity in a normal reactor situation.

Mass transport calculations are performed to determine the activity distribution in the reactor circuit. However, for Sr-90 an attempt must also be made to determine the physical form of the deposit in order to analyze accident situations, e.g., the rapid depressurization accident which could lead to ejection of Sr-90 from the reactor containment. The revolatilization of Sr-90 from metal surfaces is relatively negligible. Therefore, the major concern is with aerosols or with the fluid shear removal of dust particles containing Sr-90. If a plant is constructed so as to preclude the release of Sr-90 from the containment, the Sr-90 distribution is of minor importance to the reactor design.

The good retention of strontium, as well as of other fission product metals in HTGR fuel elements, is confirmed in the Peach Bottom reactor. The fractional release of Sr-90 into the main coolant after 300 effective full power days was measured to be 1×10^{-6} , even with essentially uncoated fuel particles.⁵ This release of Sr-90 can be completely accounted for as decay from the Kr-90 precursor.

NONVOLATILE AND REFRACTORY METAL RELEASE

Most of the remaining fission product metals are retained in high degree by the fuel and are essentially nonvolatile at normal reactor operating temperatures. This is shown, for example, by the ORNL retention data for Zr-95, Ce-144, and Ru-106 given in Table 1. Ruthenium-106 and Zr-95 are often used to determine the total amount of fissions that have occurred in a

*The reference diffusion coefficient data for strontium in graphite are described by the equation $\ln D = 5.7 - \frac{3.0 \times 10^4}{T}$.

reactor experiment; i.e., the assumption is made that there is essentially no loss of these species during the experiment. The data in Table 1 verify this assumption.

HEAVY METAL RELEASE

The uranium and other heavy metals are also relatively nonvolatile at normal reactor operating conditions. However, at high temperatures there is some amount of migration into the pyrolytic carbon coatings on the fuel particles. The only important effect should be an increase in the halogen and noble gas release from the fuel particles. This could be important since, in a properly designed reactor system, the predominant source of activity is from the halogen and noble gas precursors of the metallic fission products.

Figure 12 shows experimental data on the release of noble gases from particles subjected to a high-temperature anneal until there is some uranium migration through the pyrolytic carbon coating.¹⁹ The data points in the upper right portion of the figure represent the release from fuel particles in a carbon matrix apparently saturated with uranium. The lower point at the right is the release from the particles alone, i.e., from saturated pyrolytic carbon coatings. It can be seen that the smaller granule sizes of the carbon have led to an increased R/B by providing a shorter diffusion distance for the fission products (distributed relatively uniformly by the recoil process). Thus, the release from these "failed" particles, when adjusted for the difference in the matrix granule sizes, would be comparable to the end-of-life releases plotted in Fig. 5. Therefore, this experiment does not represent a new failure mechanism, but rather is an illustration of the surrounding granule size effect on noble gas R/B values.

SUMMARY

In a well-designed reactor system the fission product activity can be easily held to acceptable levels and will result primarily from the release of noble gases and halogens from the fuel elements. The gaseous activity will consist almost entirely of noble gases, since the halogens will be

removed (by plateout) very effectively by the loop components. This plateout activity distribution can be reasonably approximated if sufficient experimental information is obtained.

The other fission products, provided normal precautions are taken, will not be released from the fuel elements in quantities large enough to significantly increase the plateout level above that resulting from the gaseous precursors. Some of these precautions are: (1) to limit fuel element temperatures, (2) to minimize fuel particle failure, and (3) to provide sufficient sorptive material to retain the metallic fission products. A silicon carbide coating on the fuel particles is also an effective primary barrier to the release of metallic fission products.

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Table 1. Retention of Fission Product Metals from Coated and Uncoated Fuel Particles During Irradiation (ORNL Data)

Expt. No.	Fuel	Coating	Max Fuel Temp (°C)	Burnup (%)	Fraction Found in Fuel					
					Zr-95	Ce-144	Sr-89	Ba-140	Cs-137	Ru-106
A9-13	UO ₂	Buffer	1200	5.0	0.96	0.96	NA ^a	NA	0.79	0.96
B9-36	UC ₂	Bare	1200	3.5	0.95	NA	0.067	0.21	0.56	0.98
A9-12	UO ₂	Bu-Iso ^b	1570	5.0	1.0	0.93	0.24	NA	0.37	0.95
B9-33 ^c	UO ₂	PyC	1250	6.0	1.0	1.0	0.53	0.56	0.97	NA
B9-35 ^d	UO ₂	PyC-SiC	1250	5.0	1.0	1.0	NA	1.0	1.0	NA

^a NA denotes not available.

^b Failed

^c End-of-life R/B for Kr-88 was 3×10^{-5} .

^d End-of-life R/B for Kr-88 was 10^{-8} .

Fig. 1. Steady-State R/B Values vs Half-Life and Coating Contamination.

Fig. 2. Kr-85m R/B Values vs Burnup.

Fig. 3. R/B Values vs Half-Life for 6F2 (Cell I) and GAIL IIIB.

Fig. 4. R/B Values vs Half-Life for GAIL IV and Peach Bottom.

Fig. 5. Kr-85m R/B Values Corrected to 1300°C.

Fig. 6. Total Xenon Released Compared with Xenon Released as Iodine from Cracked Coated UC₂ Particles at 1400°C in ORNL Experiment B9-43.

Fig. 7. Calculated Iodine Distribution in a Reactor Circuit.

Fig. 8. Fraction of Cs-137 Released vs Annealing Time.

Fig. 9. Cesium Profiles in Peach Bottom DO6-01 Fuel Element.

Fig. 10. Cesium Concentration in GAIL IV (Type 57KBLC Particles Were Closer to GETR Core).

Fig. 11. Strontium Diffusion Profiles at 1000°C.

R/B OF
Kr-85m
AT
1200°C

Fig. 12. Steady-State Kr-85m R/B Values vs U-235 Concentration in the Carbon Matrix.

DISCUSSION

R. H. Flowers: You showed a number of results on the R/B ratio for Krypton 85 from different kernels, all being close to 10^{-2} . Was there a sol-gel kernel among them?

F. E. Vanslager: I am not sure.

R. H. Flowers: Have you had any experimental confirmation of results obtained with the FIPER code on problems involving an evaporation step.

F. E. Vanslager: At this time the comparison of FIPER with experimental results is limited to diffusion profiles.

E. Formann: Do you relate release of metallic fission products to a graphite specification?

F. E. Vanslager: No. In general we do our experimentation on the kind of graphite that will be used in the actual reactor. Therefore, we have removed this as a variable and have no need to specify fission product retention properties to the graphite manufacturer.

H. J. deNordwall: The observed Sr diffusion coefficients are not precise enough to distinguish differences between graphites. However, since data for pyrocarbon, all structural graphite, and powdered carbon are presently represented by the same line describing the variation of D with temperature, we feel that differences between graphites will be minor.

F. E. Vanslager: I agree.

D. B. Trauger: Previously there have been predictions that cesium iodide might be the prevailing form for these two elements. What is the present thinking?

F. E. Vanslager: Because of the steeply sloping concentration profiles measured in the GAIL IV and Peach Bottom elements, the present thinking is that the cesium migration is more likely to have been as elemental cesium rather than as cesium iodide.

H. J. deNordwall: Recombination of Cs and I does not seem to occur in the gas phase.

F. P. Ashworth: Dragon plate-out probe data indicates that iodine exists in the coolant in the monoatomic form with a small amount (say 10%) contribution from a larger molecule such as I_2 .

L. R. Zumwalt: A point for clarification: Would uranium migration through fuel coatings post a problem regarding fission product release at the fuel temperatures contemplated for normal HTGR operation?

F. E. Vanslager: No. The data presented in the slide giving uranium migration through the particle coatings were obtained by heating particles to temperatures above 2000°C . At reactor temperatures of the order of 1300°C ., the uranium migration should be negligible.

H. J. deNordwall: What is the evidence for the Iodine 131 activity in the gas phase as distinct from the surface concentration (falling as predicted)?

F. E. Vanslager: There were plate-out probes located at three different positions in the GAIL loop. From these probes the partial pressures of Iodine 131 could be calculated at these positions. Invariably the lowest temperature probe indicated a large reduction in gaseous iodine concentration due to plate-out in the heat exchanger.

E. Hoinkis: In your diffusion equation there is no term to take into account trapping of diffusing atoms. Could you please comment on this?

F. E. Vanslager: The equations presented are relatively general equations, and, of course, cover the two cases of trapping proportional to the concentration or of constant trapping. Other types of trapping terms are included whenever needed.

F. H. Neill: Where do you believe is the greatest need for additional experimental data for the complete understanding of fission product transport and deposition?

F. E. Vanslager: Personally, my greatest need at this time is for detailed fission product distributions in loops and reactors in order to establish the constants for the mass transport codes. Additional information on the physical form of the fission product deposits is also needed, since this can greatly affect the distribution of activity.

R. E. Blanco: I would like to ask the same question I asked Dr. Ashworth. What is the mechanism and buildup rate of tritium in the gas coolant? I realize the release limits for tritium are high, but they may be lowered in the future. Are there any plans for tritium removal from the gas coolant?

F. E. Vanslager: The three major calculated sources of tritium in an HTGR are from lithium contamination, ternary fission, and He-3 activation. In general the amount of tritium actually found in the coolant is a very small fraction of the calculated amount produced. The graphite and fuel would appear to be quite effective in retaining tritium. A typical HTGR purification system is designed to remove hydrogen (tritium) from the circulating coolant, and disposal can normally be accomplished in any desired manner without undue complications.

C. B. von der Decken: We did measure tritium in the primary and secondary circuit of the AVR. Estimates have shown that the quantity corresponds to the sources, i.e. by fission and by lithium impurities. As already mentioned we are doing experiments which should make clear the process of diffusion into the secondary systems.

F. P. Ashworth: Tritium concentrations measured in Dragon vary between a factor of 10 and 50 lower than values predicted from the lithium impurity in the graphite.

J. D. Hart: What are the chemical forms of the released fission products? Would these forms be dependent upon the other impurities in the coolant, and would chemical form affect plate-out and lift-off characteristics.

H. J. deNordwall: We can only guess at present. Association of metal fission product atoms with other atoms must affect cohesion, so correct identification of molecules is important.

SHEAR FAILURES IN END SLABS OF
PRESTRESSED CONCRETE PRESSURE VESSELS

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398 1000

ABSTRACT

This report describes the behavior of six small-scale PCRV's pressurized internally to failure. The primary object of the tests was to investigate the influence of penetrations on the shear strength of the end slab. Measured strains and calculated stresses in the end slab are discussed quantitatively to "establish" the mechanism of the observed failures.

INTRODUCTION

A series of tests of small-scale pressure vessels described in reference 1 demonstrated that the flat end slab of a cylindrical prestressed concrete pressure vessel tends to resist the internal pressure as an inverted dome after the initiation of cracking. Thus, the ultimate capacity of the end slab is related to the strengths of the essential elements of the dome system. The strength of the system may be limited by (a) the fracture of the longitudinal reinforcement, which provides the vertical reaction, (b) the fracture of the circumferential reinforcement, which provides the hoop forces, and (c) by failure of the concrete in the dome.

In the series of vessels tested, the slab failure in mode(c) was associated with the development of inclined cracks which carved a dome out of the thick end slab leading to high normal and shearing stresses in the concrete. The presence of penetrations in an end slab susceptible to inclined cracks was expected to aggravate the stress conditions in the dome. The series of tests and analysis briefly reported in this paper were carried out primarily to investigate the influence of penetrations on the strength of the end slabs failing in shear.

TEST VESSELS AND PROCEDURE

Dimensions.--The dimensions of the test vessel are shown in Fig. 1. Vessel PV16 had a solid end slab while the end slabs in the other five specimens were perforated with 2-in. or 4-in. diameter penetrations as indicated.

Concrete.--The concrete was made with Type III cement, crushed limestone (one-in. maximum size), and Wabash River sand. The average compressive strengths for each vessel determined by tests of 6 by 12-in. cylinders are listed in Table 1. Tensile strengths, listed in the same table, were based on splitting tests of 6 by 6-in. cylinders.

Prestressing.--The circumferential prestressing force was provided by 0.25-in. round high tensile strength single wire wrapped continuously around the vessel at a spacing of 0.25 in. The effective prestressing forces in the wire are listed in Table 1.

The longitudinal prestressing force was provided in each vessel by 60 Stressteel rods having a diameter of 0.75 in. Table 1 lists the total effective force for each vessel.

Seal.--A 1/16-in. thick neoprene liner was used to seal the test vessels. A stiffened 0.5-in. steel plate closed the openings on the pressurized surface of the end slab. The plate reacted against the inner slab surface.

Measurements.--In addition to the pressure in the vessel and deflections of the outer surface, strains were measured on the prestressing reinforcement, on the inner and outer surfaces of the end slab, and on the walls of the penetrations. Crack trajectories were recorded by observing the outer surface of the end slab during the test and by examination of the debris after the test. Trajectories of the inclined cracks in the slab were established by cutting sections in the end slab if the end slab survived the test in one piece.

Pressurizing.--The test vessels were initially filled with water and then pressurized with nitrogen gas. The pressure was applied in increments ranging from 250 to 100 psi. Each test took approximately six hours.

BEHAVIOR OF THE TEST VESSELS

The response characteristics of the test vessels with perforated end slabs differed little from those of the test vessel with the solid end slab. The observed influence of various patterns and sizes of penetrations on the strength of the end slab, the mechanism of failure, and over-all stiffness was small in relation to the proportion of the slab cross section removed. A general perspective of the test results behavior may be obtained from a comparison of the pressure-deflection curves in Fig. 2 with the help of the information in Fig. 1.

All six vessels failed in shear as a direct result of the development of inclined cracks. In two cases (PV18 and PV19) the seal failed before the structural mechanism was completed. The other four failed explosively.

Crack Development

The penetrations in the end slab permitted the measurement of strains at various levels in the end slab. Despite the fact that these strains do not represent average deformation conditions at a given level, they do provide a vehicle for examining internal strains leading to cracking.

Evidence discussed in reference 1 implied that inclined cracks in the end slab formed initially at approximately mid-height and then propagated toward both surfaces of the slab. This implication can be checked with the help of strain measurements made in a particular test vessel, PV17.

Strains were measured on the walls of the penetration in vessel PV17 at levels A, B, and C as shown in Fig. 4. A set of individual strain measurements at level B, near the mid-depth of the end slab, is shown in Fig. 4. The strains varied smoothly and virtually at a linear rate with internal pressure up to 1500 psi. At pressures above 1500 psi, some gages indicated reversals and some gave erratic readings, indicating the development of a strain disturbance at or near level B.

Figure 5 is a representation in polar coordinates of strains at levels A, B, and C in vessel PVI7. Strains are plotted with reference to the origin. The radii indicate magnitudes and the angles with the horizontal axis indicate orientations to the radial ($r-\theta$) plane: a strain plotted along the horizontal axis represents a radial strain while a strain plotted along the vertical axis represents a vertical strain.

The solid curves refer to measured strains at a given level derived from the averages of the strain measured in three directions. The broken curves indicate the estimated cracking strain, or the sum of the calculated strain caused by the prestress and a strain of 30×10^{-5} assumed as a reasonable upper bound to the cracking strain for concrete. The curves for the measured strain and cracking capacity should be more properly treated as bands rather than lines because of the expected scatter.

Comparison of the solid curves with the broken curves provide information on the likelihood and orientation of cracking at various levels in the end slab. The curves in Fig. 5 indicate that the likelihood of cracking was small at an internal pressure of 1000 psi. At 1500 psi, it appears that cracking was most likely at level B and would form at an angle of approximately 45° with the horizontal. The observation that the inclined crack was likely to start at midheight at an internal pressure of 1500 psi confirms the indications of indirect evidence from other test vessels which developed inclined cracks.

Failure Mechanism

The three major crack systems, radial, inclined, and reentrant corner cracks, form at about the same pressure level.

The initial change in slope of the pressure-deflection curves in Fig. 2 at approximately 1500 psi is attributable primarily to cracks at the reentrant corner between the slab and the side wall and not to the development of the inclined cracks. Radial cracks on the outer surface of the slab formed as the pressure was increased above 1500 psi. Indirect evidence indicates that the inclined cracks when they began to propagate,

did so rapidly, carving an "inverted dome" out of the end slab (Fig. 3, PV18). At pressures above 3000 psi, the moment on the wedge shaped sectors of the end slab, bounded by the radial cracks, tends to overcome the "clamping" moment on these sectors exerted by the prestressing forces. Consequently, another change in slope occurs. The rotation of these sectors about the edge of the test vessel would lead to a flexural failure but stresses in the inverted dome caused a failure in shear at a pressure and deflection lower than those that would have been developed as a result of flexural failure.

The "typical" characteristics of a shear failure in a solid end slab can be illustrated in reference to the critical crack trajectory in vessel PV18, Fig. 3. The inclined crack formed at an angle of approximately 45° to the horizontal near the mid-depth of the end slab. As this crack approached the pressurized surface, the radial compressive stresses, as well as the shear and circumferential stresses, increased either to direct the crack to a nearly horizontal path or to cause failure. The crack trajectory observed in PV18 and PV19 indicates that penetrations did not change this pattern critically. The crack trajectories shown for vessels PV17, PV20, and PV21 were reconstructed from the debris after explosive failures and do not necessarily indicate that the inclined crack had not extended beyond mid-depth at the time of failure. It appears quite likely that the final collapse occurred as a result of high stresses on a circumferential surface containing the penetrations, rather than at a point closer to the pressurized surface as in the case of PV16. A quantitative discussion of stress combinations in the "inverted dome" carved by the inclined cracks is presented in the next chapter.

STRESSES IN THE END SLAB AFTER DEVELOPMENT OF THE INCLINED CRACKS

The available experimental evidence indicates very strongly that "shear" failures of the end slabs tested was ultimately caused by the material failure of the concrete under a complex state of compressive

stresses in the three orthogonal directions and shear stresses in the radial-vertical plane. Assuming that the shape of the inverted dome carved by the inclined cracks is known, to determine the failure capacity of the dome it is necessary to calculate all components of the stresses in all sections of the dome and compare the resulting combinations with a failure criterion which takes into account properly the interrelationship between these stresses. To make such a calculation with any claims to certainty or precision is impossible within the context of available knowledge. The following analysis is made primarily with the objective of investigating whether the estimated stresses in the end slab at maximum pressure are in a range consistent with the observed strength of concrete subjected to multi-axial stresses. The analysis is made for a specific test vessel at the maximum internal stress measured.

Figure 6 shows one-half of a cross section through the end slab after an idealized inclined crack has carved out a complete inverted dome. The stresses in the dome are determined with the finite-element method, assuming the slab is linearly elastic, for an internal pressure of 3200 psi. The vertical and horizontal forces in the reinforcement are introduced as indicated in Fig. 6 and correspond to values measured in vessel PV16 at ultimate pressure.

Variations along the radius of calculated stresses at midheight of the dome section are plotted in Fig. 7 for the radial, tangential, and vertical normal stresses and the shear stress in the radial-vertical plane. Distributions of these stresses over the depth of the dome section at two different radii are shown in Fig. 8.

Certain features of the calculated stress distributions are noteworthy with a view to simplifying the interpretation of the stress conditions in the dome.

(1) The forces resulting from the integration of the radial and tangential stresses over the depth of the dome section remain essentially constant along the radius.

(2) At points away from the center of the dome, the variation of the radial stress over the depth of the dome section is small.

(3) The shear stress at the midheight of the dome section is comparable to both the nominal shear stress and the maximum value indicated by the fine-element solution.

(4) At points away from the center of the dome, the variation of the vertical stress over the depth of the dome section is very small.

In effect, the critical stresses at any radius could be considered simply as the average radial stress, based on the radial force obtained by assuming a constant distribution of tangential force along the radius; a shear stress equal to the nominal shear stress at that section; and the vertical stress equal to the pressure on the inner surface of the dome.

The high radial and tangential stresses near the center of the dome are extraneous to the real problem. Solutions of the same dome with lower moduli for the concrete within two in. of center indicated that these stresses would be reduced as a result of nonlinear response without affecting the stresses elsewhere critically. In fact, this portion of the dome could be deleted without critical effect on the stresses in the dome.

Assuming that Mohr's failure criterion applied to concrete under combined stresses, a failure envelope can be constructed using the following information: (a) A Mohr's circle representing the stress condition for the cylinder-splitting test, (b) a Mohr's circle representing the stress condition for the uniaxial compression test, and (c) an approximate slope for higher values of the principal compressive stress obtained from the relationship derived by Richard et al (Ref. 2)

$$\sigma_1 = f'_c + 4.1 \sigma_2 \quad (1)$$

where σ_1 = applied axial stress interpreted as the maximum principal stress,

f'_c = compressive strength of the concrete,

σ_2 = confining stress interpreted as the minimum principal stress.

The stress conditions at various levels for sections in the dome at radii of 4.88 and 9.5 in. from the dome center are shown in Fig. 9. The comparison of the Mohr's circle drawn for the stress conditions and the envelope suggest only that, at the time of failure pressure in vessel PV16, the magnitudes of the calculated stresses are plausible in relation to the information available about the strength characteristics of concrete under multiaxial stresses. It also appears conversely that Mohr's failure theory can be used to determine the strength of the end slab. This conclusion also promises a lead to the explanation of the relatively small variation between the ultimate capacities of the slabs with and without penetrations failing in shear. The introduction of a penetration would increase the radial and shear stresses at approximately the same rate. Consequently, although the shear stresses would increase, so would the shear strength. Work is currently in progress toward the testing of this hypothesis both experimentally and analytically.

ANALYSIS OF STRESS CONDITIONS LEADING TO INCLINED CRACKS

In order to project the results of the experimental investigation to combinations of variables not directly covered in the tests, it is necessary to develop a rational method for predicting the crack trajectories as well as analyzing the stresses in the end slab after the cracks have formed. To this end, the lumped-parameter method developed by Echeverria and Schnobrich (Ref. 3) is being extended to predict the trajectories of inclined cracks. Analysis of possible crack formations in vessel PV16 using the lumped-parameter model with a limiting-strain criterion for tensile failure of the concrete is shown in Fig. 10. In addition to the radial and tangential cracks attributable primarily to flexural stresses, the analysis predicts inclined or conical cracks at approximately 45° which flatten as they approach the pressurized surface.

SUMMARY

The test data from the six prestressed concrete pressure vessels indicate no significant reduction in strength as a result of penetrations through the head. Three major crack systems develop in the head during pressure loading. These involve radial cracks beginning at the top center of the head, inclined cracks originating at mid-depth on a forty-five degree line from the end anchorage down toward the center of the vessel, and conical cracks at the reentrant corner. These cracks change the flat head of the vessel structurally to that of an inverted dome. A Mohr's circle failure criterion seems plausible for predicting the strength of this inverted dome. An explanation for the lack of effect of penetrations on the strength is, that these penetrations increase the shear and radial stresses in the same proportion, thus increasing the strength characteristics of the concrete as well.

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Table 1

Mark	Circum. Prestress Force per Wire	Long. Prestress Total Force	Concrete		Prestress Index ^a	Index ^b	Maximum Pressure Reached
	kips	Kips	Comp. Strength psi	Tens. Strength psi	Circum. ^a psi	Long. psi	psi
PV16	6.03	1995	7450	518	1930	4060	3200
PV17	6.33	2080	7180	534	2030	4240	3000
PV18	5.94	1818	7590	447	1900	3700	3000
PV19	5.85	2300	7470	406	1870	4690	3500
PV20	5.77	2140	6890	469	1850	4360	3300
PV21	5.65	2300	7400	496	1810	4700	3300

^aRatio of total circumferential prestressing force per unit height of vessel to the internal diameter.

^bRatio of total horizontal prestressing force to area of cavity at a transverse section.

(a) Cross Section

(b) End Slab Penetrations

FIG. 1 TEST VESSELS

FIG. 2 PRESSURE-DEFLECTION CURVES

FIG. 3 CRACKS LEADING TO FAILURE

FIG. 4 STRAINS MEASURED AT LEVEL B, TEST VESSEL PV17

FIG. 5 STRAINS AT VARIOUS LEVELS IN THE END SLAB OF PV17 IN POLAR COORDINATES

FIG. 6 SECTION OF IDEALIZED INVERTED DOME

FIG. 7 VARIATION OF CALCULATED STRESSES ALONG THE RADIUS OF THE DOME

(a) SECTION AT $R = 4.88$

(b) SECTION AT $R = 9.5$

FIG. 8 VARIATION OF THE CALCULATED STRESSES OVER THE DEPTH OF THE DOME

FIG. 9 ESTIMATED STRESS CONDITIONS AT FAILURE OF VESSEL PV16

FIG. 10 CRACK TRAJECTORIES PREDICTED BY THE LUMPED-PARAMETER ANALYSIS IN VESSEL PV 16

DISCUSSION

G. D. Whitman: The absence of cross head tendons in the top head does not necessarily impair the strength of the head since the circumferential tendons induce the prestressing forces required to achieve stability against internal pressure loads.

INVESTIGATIONS OF TIME-DEPENDENT BEHAVIOR OF
PRESTRESSED CONCRETE PRESSURE VESSELS*

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6/7/60

ABSTRACT

The effects of the thermal conditions imposed on a prestressed concrete reactor vessel (PCRV) present a major uncertainty in predicting long-term vessel behavior. Research and development programs are under way to provide adequate analytical methods for predicting long-term behavior and to provide the basic materials data needed to effectively utilize such methods. It is generally agreed, however, that ultimately the validity of the analytical methods and materials data obtained from small specimens must be demonstrated by designing, analyzing, and testing a realistically sized and shaped structure under conditions similar to those in an actual vessel.

Thus, a thermal cylinder model, which simulates a portion of the barrel section of a cylindrical vessel is being built and will be tested at Oak Ridge National Laboratory. The thick-walled cylinder specimen is 48 in. high, 18 in. thick, and 81 in. OD; it will be made of the same material as used in the basic tests, and it will be sealed to prevent moisture loss in the same manner as the basic specimens. The model is prestressed axially and circumferentially, and provision is made for applying an internal pressure loading and a temperature profile simulating thermal conditions in an operating PCRV. The specimen will be subjected to mechanical and thermal loading histories that simulate, on a compressed time scale, the loading histories that an actual vessel might experience during the first few years of operation. Near the end of the test the specimen will be subjected to off-design hot spot conditions to obtain information on the capability of the structure to endure prolonged accidental over-temperatures. The model will be instrumented to measure stress, strain, temperature, and moisture levels at selected locations.

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This paper briefly discusses the basic materials investigations that are being carried out and presents some initial results and trends. The thermal cylinder test is then described and results from the preliminary design creep analysis of the test specimen are presented.

INTRODUCTION

The single most important factor making prestressed concrete reactor vessels (PCRV's) unique in the technology of prestressed concrete is the long-term thermal condition imposed on the structure by the relatively high-temperature reactor process fluid. In present vessels, a complex system of insulation and cooling is provided to limit maximum concrete temperature to 200°F or less, but the long-term behavior of the vessels can be significantly affected by temperatures of even this relatively low level. Over the 30-year design life of a concrete vessel, these thermal effects present a major uncertainty with regard to understanding and accurately predicting long-term vessel behavior and margins of safety.

The factors that are of primary concern to the vessel designer are thermal stresses, concrete creep and shrinkage, and changes in the mechanical and physical concrete properties with time. Each of these factors is dependent on temperature and moisture levels and/or distributions, and these are, in turn, interrelated in a complicated way. Creep and shrinkage can have serious consequences for a concrete vessel because of the loss of initial prestress, because of possible large deformations of the vessel resulting, for example, in misalignment of control rod and fueling passages, and because of residual stresses that may be introduced as the loading conditions change. In the latter case, the residual stresses could conceivably be tensile stresses and could be coupled with inferior concrete strength properties, which may have come about over a period of time, resulting in serious damage to the vessel.

In 1965, the Oak Ridge National Laboratory was asked by the United States Atomic Energy Commission to formulate and direct a long-range basic research and development program that supports the technology of

prestressed concrete vessels for nuclear reactors. A major portion of this program^{1,2} has been aimed at the problems described above. A two-pronged approach is being followed to provide vessel designers with the information and tools needed to understand and predict long-term vessel behavior. On the one hand, basic materials investigations are under way to provide multiaxial concrete creep data at room and elevated temperatures for typical concrete mixes; moisture distribution tests on small simulated vessel segments are being performed; physical and mechanical properties data are being obtained to characterize the concrete mixes used; and the effects of sustained and cyclic elevated temperatures on these concretes are being studied. These investigations, in which, for the most part, individual variables are being investigated using small specimens, were formulated to provide much of the basic information necessary to separate the various interrelated effects associated with long-term vessel behavior.

The second part of the overall approach is the development of complex analytical methods for predicting the creep, cracking, and ultimate strength behavior of concrete vessels. Hopefully, the basic materials data can be utilized together with these analytical methods to correctly predict the long-term interrelated effects on a concrete vessel. However, to ultimately demonstrate the validity and accuracy of the analytical techniques and basic materials data, a realistically shaped structure must be designed, analyzed using the newly developed techniques and data, and subjected to test conditions simulating those in an actual vessel.

To meet this need, a relatively small thermal cylinder test structure is being built and will be tested at ORNL. The test specimen is a thick-walled cylinder 48 in. high, 18 in. thick, and 81 in. OD, and it represents a segment out of the cylindrical portion of a cylindrical concrete vessel as shown in Fig. 1. The specimen will be subjected to prestressing, internal pressure, and thermal loading histories that simulate, on a compressed time scale, the loading histories that an actual vessel might experience during the first few years of operation. Near the end of the test, the specimen will be subjected to off-design hot spot conditions to evaluate the capability of the structure to endure accidental over-temperature conditions.

Fig. 1. Thermal Cylinder Test Specimen Relationship to Prestressed Concrete Reactor Vessel.

Table 1. Unconfined Compression Test Results

Curing Condition	28-Day Cure			90-Day Cure		
	Elastic Modulus (10 ⁶ psi)	Poisson's Ratio	Ultimate Strength (psi)	Elastic Modulus (10 ⁶ psi)	Poisson's Ratio	Ultimate Strength (psi)
Immersed	5.4	0.24	6460	6.1	0.26	8310
As cast	5.6	0.26	6010	6.3	0.26	6870
Air dry	5.3	0.26	6590	6.0	0.26	7560

The specimen will be extensively instrumented to measure strains, stresses, temperatures, and moisture levels with time. The concrete used for the specimen will be the same as that used in the basic materials investigations, and all surfaces of the test structure will be sealed to duplicate the moisture condition of the sealed specimens in the basic studies. Thus the basic materials data can be used in conjunction with inelastic time-dependent structural analysis techniques to predict the time-dependent behavior of the test structure. The accuracy of the techniques used will then be evaluated by comparing the predictions with the test results.

The following section of this paper briefly discusses the basic materials investigations that are being carried out. Some initial results and trends are discussed. The thermal cylinder test is then described and discussed in the third section and typical results from the preliminary design creep analysis are presented. Finally, the last section contains a brief summary and discussion of the overall integrated study.

MATERIALS INVESTIGATIONS

A concrete mixture was selected to represent what might be used in a PCRV in most sections of the United States. The mixture is composed of a 3/4-in. maximum size Tennessee limestone aggregate and Type II Portland cement. Its design 28-day compressive strength is 6000 psi. The mixture was designed and specified by the Waterways Experiment Station of the Corps of Engineers, Department of the Army, Vicksburg, Mississippi. This concrete has been used in all the basic materials investigations and is specified for the thermal cylinder experiment.

The following materials investigations are being conducted by the respective organizations in order to completely characterize the PCRV concrete:

1. Basic mechanical and physical properties Oak Ridge National Laboratory
2. Strength properties Oak Ridge National Laboratory and University of California, Berkeley

3. Creep for multiaxial loadings	University of Texas and
Waterways Experiment Station
4. Moisture movement	Waterways Experiment Station

Basic Mechanical and Physical Properties

With the exception of the moisture movement study, all tests are being conducted using relatively small cylindrical specimens representing three curing conditions - air dried, sealed at the time of casting (as cast), and moist cured (immersed). The basic measured mechanical properties and ultimate strengths are listed in Table 1 for these three curing conditions.

The basic physical properties that have been measured for the PCRV concrete are the coefficient of thermal expansion, thermal conductivity, thermal diffusivity, and specific heat.

Thermal length change measurements were made for both air-drying and as-cast conditions as follows:

<u>Specimen</u>	<u>Temperature Range</u>	<u>α(in./in./°F)</u>
Air dried	73°F - 210°F	4.3×10^{-6}
Air dried	210°F - 333°F	0.6×10^{-6}
Air dried	73°F - 333°F	2.3×10^{-6}
As cast	80°F - 147°F	3.7×10^{-6}

It can be seen that air-drying specimens experience a drastic change in the observed coefficient of thermal expansion as the concrete is heated above the boiling point since shrinkage effects are presumed to predominate in this temperature regime.

Thermal conductivity tests were conducted only on air-drying specimens. A sharp initial decrease in thermal conductivity with moisture loss was measured. The value of thermal conductivity measured at 28 days from casting was 1.25 Btu/ft-hr-°F. The specimen reached a constant value of 1.12 Btu/ft-hr-°F in 220 days. Thermal conductivity was not found to be sensitive to temperature changes below 250°F.

The thermal diffusivity of air-dried concrete was found to be 0.0274 ft²/hr, and the specific heat was 0.274 Btu/lb-°F.

Strength Properties

The average unconfined compressive strengths of 6 in. by 12 in. cylinders subjected to all three curing conditions are plotted in Fig. 2. Since the air-dried specimens for these particular tests were moist cured for seven days prior to air drying, their initial rate of strength gain was substantially higher than the as-cast specimens which were sealed in epoxy and soldered copper two days after casting. However, after 180 days the sealed as-cast specimens continued to gain strength while the air-drying cylinders experienced virtually no further increase in strength, as shown in the figure. Heating the as-cast specimens to 150°F at the end of their 7-day curing period resulted in increased strength gain at 6-months age, but their 1-year strengths were less than those of the sealed specimens remaining at 75°F. Heating the air-drying specimens caused a slight decrease in their 6-month and 1-year strengths.

An extensive study is under way at the University of California to obtain as complete an account as possible of the mechanical behavior of the PCRV concrete after various forms of thermal exposure. A major effort in the study has thus far been concentrated on the development of strain instrumentation and sealing techniques to be used at temperatures above the boiling point of water. A copper jacket has been developed for cylindrical concrete specimens that appears to be satisfactory at temperatures up to 300°F. Electric blankets are used to heat each specimen individually. Strains are measured using the Microdot embedment strain gage that is constructed of a hairpin wire filament embedded in compacted magnesium oxide powder which is in turn contained in a sealed stainless steel tube. The ends of the tube are welded to perforated steel end plates which serve to anchor the gage in the concrete.

A pilot test of sealed 6 by 12-in. concrete cylinders containing embedded Microdot gages has been completed. These specimens were cured in water at 70°F for 28 days prior to sealing in copper. Individual specimens were subjected to one of the following temperature conditions:

1. Maintained at 70°F (control specimen),
2. Three cycles of 70-300-70°F and tested at 70°F,

ORNL-DWG 70-4416

Fig. 2. Ultimate Unconfined Compressive Strength of PCR/V Concrete for Various Ages and Moisture Conditions.

ORNL-DWG 70-4415

Fig. 3. Effect of Temperature on Stress-Strain Characteristics of Sealed PCR/V Concrete.

3. Three cycles of 70-300-70°F and tested at 300°F,
4. Heated continuously at 300°F and tested at 300°F.

The stress-strain curves of specimens subjected to the first three temperature conditions are shown in Fig. 3. The stress-strain curve for the specimen continuously heated at 300°F was almost identical to the thermal cycled specimen tested at 300°F. These are preliminary results and further testing is currently under way.

Creep Under Multiaxial Loadings

The creep testing program consists of uniaxial, biaxial, and tri-axial stressing of specimens both at room temperature and at 150°F. Cylindrical concrete specimens 6 in. in diameter and 16 in. long are being tested as shown in Fig. 4. Two specimens are subjected to the same axial load in each load frame; however, their lateral loading can be different. The majority of the specimens were loaded 90 days after casting. Combinations of load, temperature, and moisture conditions included in the program are summarized in Fig. 5. The air-drying specimens were sealed in epoxy and soldered copper just prior to loading at 90 days.

Strain instrumentation consisted of vibrating wire concrete embedment strain gages cast into each specimen in both the axial and lateral directions as shown in Fig. 4.

In addition to creep specimens, identical free shrinkage specimens were included for each temperature and moisture condition. After the creep specimens have been unloaded, creep recovery measurement will be made.

Some preliminary results from the creep tests being conducted by the University of Texas are shown in Fig. 6. Figure 6(a) shows plots of the axial strain measured with time for several representative loading conditions both at 75°F and 150°F. Figure 6(b) consists of radial or lateral strain measurements with time for the same loading conditions and temperatures shown in the first plot. It should be pointed out that for every combination of loading the higher temperature resulted in significantly higher creep. A preliminary value of creep Poisson's ratio

Fig. 4. University of Texas Triaxial Creep Test Unit.

Fig. 5. Conditions for Triaxial Creep Tests.

MOISTURE CONDITIONS

1. AS-CAST SEALED SPECIMENS.
2. AIR DRY SPECIMENS SEALED JUST PRIOR TO LOADING.

STRESS CONDITIONS

- ▲ STRESSES TO BE EMPLOYED IN BOTH ROOM TEMP. AND ELEVATED TEMP. TESTS.
- △ STRESSES TO BE EMPLOYED IN ROOM TEMPERATURE TESTS.

for the as-cast specimens is 0.17 for 150°F and 0.12 for 75°F. These particular values are significantly lower than the room temperature elastic Poisson's ratios of 0.26 shown in Table 1. The creep tests are still in progress and these results should be considered to be only preliminary in nature.

Moisture Movement Experiment

Shrinkage, creep, and strength of concrete are affected by moisture content and changes in the moisture content. In order to evaluate these effects, a pie-shaped specimen 9 ft long, 2 ft deep, and end widths of 2 ft and 2 ft 8 in. simulating a moisture stream tube out of the cylindrical wall of a PCRV is being tested. The specimen was sealed with epoxy and soldered copper on the lateral surfaces and the simulated interior vessel face. The open end is exposed to the atmosphere at 70°F and 50% relative humidity. Heaters and insulations were provided so that a thermal gradient can be imposed on the block. The heaters were also used to simulate the thermal conditions that would exist in an infinitely long cylindrical vessel section by applying heat and maintaining a very small temperature difference in an equivalent circumferential plane during the initial unheated period. Carlson strain meters, thermocouples, and moisture measuring devices were cast into the concrete. The total moisture content is measured by a nuclear moisture meter, and a measure of the free water in terms of changes of the dielectric properties of the concrete is made using open wire line probes developed by Waterways Experiment Station. Monfore humidity gages are also being used in wells provided for that purpose.

The moisture movement specimen remained in unheated condition for 16 months. During this time the moisture conditions in the specimen remained relatively stable except in the immediate vicinity of the open end. In March 1970, the sealed end of the specimen was heated to 150°F. The temperature levels at various horizontal distances from the heated end are shown in Fig. 7 for periods from 16 hours to 17 days after the initiation of heating. The Carlson strain meters have indicated continual concrete expansion since heating, with changes in strain ranging

Fig. 6. Effect of Temperature, Moisture and Type of Multiaxial Loading on Creep of PCRV Concrete.

Fig. 7. Moisture Migration Study Temperature Distribution at the Center Line.

from 280 μ in./in. near the heated end to practically no change near the unheated end. There appears to be little change thus far in the moisture content of the concrete since the initiation of heating. The specimen is scheduled to continue to be heated for a period of one year.

THERMAL CYLINDER TEST

The objective of a thermal cylinder test is to simulate, as closely as possible, the middle section of the cylindrical portion of the vessel and the conditions imposed on it. The resulting test specimen is shown, in simplified form, on the right in Fig. 1. The specimen is sealed at top and bottom to eliminate any moisture flow axially, and the ends are also insulated thermally. The moisture flow and heat flow would thus be confined primarily to the radial direction, simulating conditions near the middle of the actual vessel. The specimen is prestressed axially and circumferentially, and an internal pressure can be applied to the inner surface. The temperature distribution is simulated by heating the inner surface and cooling the outer surface.

To correctly simulate the thermal gradient and the moisture gradient, the thermal cylinder specimen should ideally be near the size of an actual vessel. A design study was carried out, based on a thick-walled cylinder test specimen, to provide a basis for evaluating the feasibility and desirability, as well as the cost of performing a large-scale thermal test.³

The specimen chosen was 15 ft 9 in. high and 27 ft OD with a wall thickness of 6 ft. In addition to prestressing, the cylinder was to be subjected to a thermal gradient and an internal pressure. It was to be tested for a period of three to four years under combinations of conditions representative of an actual PCRV. The total cost for providing the test structure and the necessary facilities, instrumentation, and controls, ready for testing, was estimated to be almost \$1 million.

Thus the design study showed that a large-scale thermal test would be a large and very expensive undertaking. Furthermore, its success would be entirely dependent on the successful performance of various types of

embedded instruments whose performance had not been adequately demonstrated under the conditions that would exist. Consequently, a decision was made to proceed with a smaller thermal cylinder test to learn more about the behavior of such a specimen and the behavior of the necessary types of instrumentation. In addition to serving as a pilot test, the small thermal cylinder test will provide data to meet many of the original objectives established for the large thermal cylinder test. Since the moisture gradient could not be simulated correctly in the small specimen, it was decided to seal the specimen completely.

Specimen Description

The actual detailed test structure is depicted in Fig. 8. The thick-walled cylinder specimen is 48 in. high, 18 in. thick, and 81 in. OD. The design is based on an internal pressure of 700 psi and a normal temperature distribution ranging from 150°F on the inner surface to 75°F on the outer surface. The ends are insulated thermally with loose mineral wool fiber insulation at the top and glass foam blocks at the bottom.

The cylinder specimen is prestressed in the axial and circumferential directions with the Stressteel-S.E.E.E. strand system. The arrangement for the circumferential prestressing is, however, unique in the small thermal cylinder. Basically, it consists of monostrand tendons wrapped around the outer surface of the cylinder and then tensioned by jacking them radially outward. This system has the advantage of moving the circumferential tendons out of the wall of the cylinder where they would produce troublesome stress concentrations and also limit the space available for embedded instrumentation. It also has advantages over conventional wire winding in this case because (1) it would be difficult to wind a cylinder of this size, which is too large for a prestressed-concrete-pipe winding machine and too small for conventional tank-winding equipment, and (2) this method of tensioning allows an accurate measurement to be made of the tensioning forces actually applied to the wrapped tendons. The circumferential system is a modification of an arrangement proposed for prestressed-concrete reactor vessels by the engineering firm of Coyne and Bellier in France.⁴ The anchoring arrangement for the tendons in one

Fig. 8. Cutaway Drawing of Thermal Cylinder Test Structure.

Fig. 9. Anchoring Arrangement for Monostrand Tendons in a Circumferential Prestressing Band.

of the four circumferential bands and a nonanchor bearing plate are shown in Fig. 9.

A heat source is provided on the inner face and a heat sink on the outer face of the small cylinder in order to establish the normal vessel temperature difference of 150 to 75°F in the cross section. Process water controlled at the desired temperature will be circulated for this purpose.

Plate- or panel-type heat exchangers are provided on the inner and outer surfaces of the cylinder. The channel configuration is equivalent to one-half the flow cross section of a standard 3/8-in. pipe. This small cross section will permit the establishment of a high heat transfer coefficient for flows commensurate with the low heat transfer rate required. The flow passages, which run vertically up and down the cylinder walls, are spaced 1 1/8 in. apart around the circumference of the specimen.

On the inner surface of the specimen the embossed heat exchanger panels are fitted to a 1/8-in.-thick mild steel liner, which simulates the liner in an actual PCRV. On the outer surface, the embossed panels are fitted to a one-piece 22-gage steel sheet. The embossed panels are also made of 22-gage sheet. The heat exchanger assemblies will serve as the inner and outer formwork for the concrete and as moisture seals on the inner and outer surfaces. On the inner surface the assembly will be anchored to the specimen by 1/4-in.-diam, 1 1/2-in.-long standard-threaded Nelson stored-arc studs.

The inner concrete core, or island, is provided in connection with the pressurizing scheme. The outer surface of the core is fitted with the same 1/8-in. steel liner and embossed heat exchanger arrangement as the inner surface of the specimen, and the two liner assemblies are joined by closures at top and bottom consisting of split circular tubes welded to the liner assemblies. Thus a small-volume pressure annulus is provided for applying internal pressure to the cylinder specimen. Hydraulic oil will be used as the pressurizing medium, with the pressure supplied by bottled gas through an oil-expansion tank. The split-pipe closure at

the top of the pressurization annulus is provided with threaded connections to permit pressurization and venting and to provide for instrumentation leads to the annulus.

The heat source on the face of the core is provided to prevent heat transfer across the pressurization annulus. It will supply the heat required during heatup and that required to maintain a uniform core temperature on the inner surface of the test specimen. Since the temperature control points for the core heat exchanger and the heat exchanger on the inner surface of the specimen are identical, a split-flow arrangement is provided for the heating water, with the bulk of the flow to be directed through the specimen heat exchanger.

Thin sheet metal sheeting is provided on the bottom of the cylindrical specimen and on the bottom of the center core to serve as a part of the formwork and as the bottom moisture seal. The top surfaces of the cylinder and core will be coated with layers of epoxy and copper foil soon after the cylinder and core are cast. The epoxy-copper combination serves as the top moisture seal. In addition, all sheet metal joints and joints in the axial tendon ducts will be sealed with epoxy.

Prior to casting, the composites of the liners, the panel coil heat exchanger assemblies, the bottom sheet metal surfaces, and the axial tendon assemblies will be assembled on the glass foam insulation blocks, which in turn will be supported on a structural steel base frame that will permit handling of the entire assembly with the concrete in place.

Tentative Test Schedule

A tentative test program and time schedule for the small thermal cylinder test has been developed. Design of the test structure and procurement of the instrumentation and major components of the test structure is complete. Approximately six months of additional time will be required before the specimen is cast. The test will begin when the specimen is cast, with temperatures, moisture levels, strains, and stresses being recorded with time.

At a concrete age of 90 days, which corresponds to the age at loading of the initial creep specimens in the materials investigations, the prestressing operation will begin. It is estimated that the prestressing can be completed in two to three days.

After a one-month period during which only the prestressing loads will act on the test specimen, the heating phase will begin. The design temperature gradient will be applied by raising the temperature of the inner surface of the cylinder from 75 to 150°F at a rate of 5°F per day. The outer surface will be maintained at 75°F. Following completion of the heating phase, a period of 90 days will pass during which only the prestressing plus the thermal gradient will be acting. At the end of this period, the internal pressure of 700 psi will be added for a period of 180 days.

Following the relatively long-term pressure test period, the pressure will be removed for 45 days and then possibly reapplied for 45 days to study creep recovery and residual stress effects.

For the final portion of the test, it is tentatively planned to heat a narrow circumferential band on the inner surface of the specimen to a temperature of 450°F for a period of 90 days and to then bring the temperature of the band quickly back to normal. The purpose of this "hot-spot" test will be to evaluate the possibility of a prolonged accidental over-temperature resulting in concrete cracking when the temperature is lowered. To apply the local over-temperature, a special heating cable, not shown in Fig. 8, will be wrapped circumferentially around the inner surface of the cylinder prior to casting.

Preliminary Design Analysis

To verify the adequacy of the design, a preliminary transient thermal analysis and a time-dependent finite element creep analysis were performed. The creep analysis was used to predict the stress and strain behavior of the specimen starting with the initial prestressing and extending through the 45-day period during which the internal pressure was removed.

In actual vessel practice the temperature gradient is applied at a rate of 1°F per day, or less, with the expectation that the resulting thermal stresses will largely be relieved by creep or relaxation as the gradient is applied. In the small thermal cylinder the gradient will be applied at an accelerated rate of 5°F per day. To examine the resulting temperature distributions in the specimen with time, a two-dimensional, axisymmetric, transient heat transfer analysis was carried out, and the results are shown in Fig. 10. The distributions shown are through-the-wall thickness at the specimen midplane. The solid lines are transient temperatures, while the dashed line represents the final steady-state temperature gradient through the wall. A thermal conductivity of $1.3 \text{ Btu/hr-ft-}^{\circ}\text{F}$ and a specific heat of $0.2 \text{ Btu/lb-}^{\circ}\text{F}$ were used for the concrete. The insulation at top and bottom was accounted for by using equivalent heat transfer coefficients. Although the 5°F per day temperature rise on the inner surface of the specimen is larger than would be used in actual practice, the transient temperature curves in Fig. 10 indicate that the resulting temperature distribution at any time is essentially that corresponding to steady-state conditions.

The preliminary time-dependent creep analysis was performed using the finite element computer program SAFE-CREEP,⁵ which was developed for PCRV's by Gulf General Atomic, Inc. This is a linear viscoelastic program based on the superposition principle. It is a two-dimensional analysis that will treat either axisymmetric or plane concrete structures. A specific set of concrete creep data, which includes age at loading as a variable, is built into the program.

Although any other set of creep data can, with very little trouble, be programmed into the SAFE-CREEP program, the preliminary analysis was performed using the data currently in the program. The initial creep data from the basic creep investigations previously described indicate that the actual thermal cylinder concrete will exhibit slightly less primary creep than indicated by the data used in the computer program and about the same amount of secondary creep.

The finite element layout for the analysis is shown in Fig. 11. Both the cylinder and the core are shown divided into a finite element grid. In these structures the idealizations include membrane-type elements representing the 1/8-in.-thick mild steel liners, the panel coil heat exchanger composites, and the sheet metal bottom seals. In the cylindrical specimen the steel anchor plates for the axial tendons are represented by steel elements. The circumferential prestress loading on the cylinder was represented by an external pressure.

Typical results from the preliminary time-dependent analysis of the cylindrical specimen are presented in Figs. 12, 13 and 14. The circumferential and axial stresses on the inner and outer surfaces of the cylinder at midheight are shown in Fig. 12 as a function of time since prestressing. The heavy lines are the viscoelastic predictions while the fine lines indicate the variations predicted by a purely elastic analysis. The circumferential and axial strain variations at the same points are shown in Fig. 13, while the circumferential and axial stresses in the steel liner at midheight are shown in Fig. 14.

While the analytical technique used for the preliminary analysis is only one of several possibilities that will be utilized in the thermal cylinder investigation, the results presented in Figs. 12, 13 and 14 do give an indication of the general stress and strain behavior expected during the test. The results show that during periods that are predominantly load controlled (for example, when only the prestressing forces are acting) the time-dependent behavior is primarily creep with the strains changing more rapidly than the stresses. During periods that are predominantly strain controlled (for example, when the thermal gradient plus the prestressing is acting) the behavior is primarily relaxation with the stresses changing more rapidly than the strains.

The effect of stress relaxation on the stresses due to prestressing plus the thermal gradient can be seen in the circumferential stress on the inner surface in Fig. 12. Calculated on an elastic basis, the stress is almost 3200 psi compression. Principal compressive stresses are generally limited to $0.45f'_c$, where f'_c is the specified 28-day compressive

Fig. 10. Temperature Distributions with Time Across Wall of Thermal Cylinder at Midheight.

NOTE: DIMENSIONS ARE IN INCHES

Fig. 11. Axisymmetric Finite Element Layouts for Thermal Cylinder Test Specimen and Core.

Fig. 12. Thermal Cylinder Test Specimen and Core-Predicted Circumferential and Axial Concrete Stress Variation with Time on Inner and Outer Surfaces.

Fig. 13. Predicted Circumferential and Axial Concrete Strain Variation with Time on Inner and Outer Surfaces of Thermal Cylinder at Midheight.

strength and is 6000 psi for the specimen concrete. Thus the compressive stress limit is 2700 psi, which is exceeded by the circumferential stress calculated on an elastic basis. With stress relaxation taken into account the maximum stress is 2550 psi, compression, which is below the limit.

This is generally the case in actual PCRV's. Early designers found that for a design to remain within practical and economic limits, account had to be taken of the beneficial effects of creep. Some designers have used various methods to approximately account for the effects of creep in their stress analyses, notably by simply using a reduced elastic modulus value, while others have relaxed the allowable stress limits for thermal plus mechanical load stresses calculated on an elastic basis.

To accurately predict the actual stresses, more realistic and rigorous creep analysis methods must be used. The linear viscoelastic method employed by SAFE-CREEP is one possibility. It is known, however, that the principle of superposition used in SAFE-CREEP does not agree, in all cases, with experimental observations on concrete behavior, and, in fact, one of the objectives of the thermal cylinder test is to evaluate the accuracy of this method.

Other analytical techniques, including a viscoelastic analysis with a modified superposition principle, the simplified effective modulus method, and an analysis based on the rate of creep method will also be investigated and utilized for comparisons with the experimental results from the thermal cylinder test.⁶ From these comparisons the most suitable creep theory and method of analysis will be identified.

Instrumentation

One of the basic objectives of the small thermal cylinder test is to evaluate various types of instrumentation. The information to be obtained from the test consists of strains, stresses, temperatures, and moisture levels throughout the concrete cylinder, as well as the concrete core; strains on the mild steel liner; forces and strains in the prestressing tendons; and overall deflections of the cylinder. With this information obtained as a function of time throughout the test, adequate comparisons can be made with analytical predictions such as those just shown.

As nearly as possible the instrumentation will lie along four radial lines, 90° apart, near the midheight of the structure. Two of the lines will pass through bearing plates of the circumferential prestressing system, while the other two will pass between bearing plates. Each pair of lines will contain identical instrumentation and will thus provide duplicate sets of data.

A total of 231 electrical instrumentation points is planned. These consist of the following:

	<u>Number to be Used</u>
Embedded vibrating-wire strain gages, including resistance thermometers in each gage	30
Embedded Microdot resistance strain gages	45
Embedded Carlson-type resistance strain gages	6
Microdot weldable resistance strain gages on the mild steel liner of the specimen	16
Embedded stress gages (three different types)	18
Resistance strain gages on prestressing tendons	20
Load transducers on axial prestressing tendons	12
LVDT's for measuring deflections	20
Thermocouples	33
Pressure transducer	1

A Troxler probe-type neutron and gamma-ray back-scattering instrument will be used for measuring moisture levels in the test cylinder and core. Vertical holes lined with thin metal tubing will be cast in at several locations where moisture measurements are desired. The probe will be lowered into the concrete from the top of the test structure to predetermined vertical positions.

Of the three types of embedment strain gages being used in the test, the vibrating wire and Carlson-type gages have had widespread use; however, the vast majority of experience has been for ambient conditions. The

Fig. 14. Predicted Circumferential and Axial Stress Variation with Time in Steel Liner at Midheight.

Fig. 15. Relaxation Test Loading Frame.

Microdot embedment gage has been developed specifically for use in concrete at elevated temperatures.

The stress gages that have been thus far used for embedment in concrete were unsatisfactory for this test primarily due to their large size and limited stress range. The Aerojet-General Corporation has developed a stress gage specifically for this application. It consists of two independent bonded strain-gage bridges in a sealed steel case. One of the bridges is designed to measure relatively small stress differences over a preset 1000 psi stress range. Because of the extreme conditions of environment and treatment to which the gages are subjected both at the time of casting of the concrete and in the approximately two-year duration of the test, two additional types of stress gages are being developed at ORNL.

The success of the thermal cylinder test depends to a large extent on the reliability and accuracy of the gage employed in measuring time-dependent stresses and strains in the concrete. Two concrete cylinders, 18 in. in diameter and 40 in. long, will be cast with the thermal cylinder in order to provide a basis for judging stress cell performance while embedded in concrete. These additional cylinders will contain representative embedment stress and strain gages of every type to be included in the thermal test and will be sealed after casting. One of the cylinders will be loaded incrementally in the frame shown in Fig. 15 in order to provide a calibration of embedded stress and strain gages. Eventually both the stressed and unstressed cylinders will be heated to 150°F and the incremental loading of the one cylinder will be repeated. Upon reaching a predetermined maximum stress, the nuts on the threaded Stressteel bars beneath the middle steel plate, Fig. 15, will be tightened to maintain the loading produced by the hydraulic ram. Once the ram has been unloaded the bars will not be retensioned during the extended test period. The embedded stress cells will be used to read any eventual change in the stress due to concrete relaxation. Both the stressed and unstressed specimens will remain at the 150°F temperature during the period of the relaxation test.

SUMMARY

The thermal cylinder experiment is one of the major projects in the program of PCRV research and development since important contributions will be made to the further understanding of long-term vessel behavior. Basic concrete materials property data utilized with advanced analytical methods will be used to predict the stresses and strains in a model that will be subjected to a simulated history representing the major loading regimes of an actual PCRV including potential off design thermal loadings.

Aside from the fundamental objective of establishing the adequacy of the basic information used in predicting vessel behavior considerable data will be obtained on the performance of prestressing hardware and special instrumentation. The instrumentation requirements present a considerable problem and the evaluation of the performance of the specified devices will in itself be of major importance to the overall program of PCRV development.

Because of the substantial costs associated with performing a test on a large-scale thermal model the small vessel model was selected as a very reasonable alternate for experimental study at this stage of technological development. The experiment has been carefully planned to provide a comprehensive check on basic information which is presumed to be applicable to the determination of large structure behavior. The information obtained from this experiment will provide important data which can be used to make more quantitative assessment of the strength and dimensional stability of concrete vessels and the factor of safety against failure from any materials degradation from the thermal environment.

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DISCUSSION

D. B. Trauger: It has been postulated that for certain liner failures it may be possible for helium to penetrate the concrete, and hence, effectively increase the loaded area and even cause failure. What is your opinion of this postulation?

T. A. Jaeger: There are five limit states of a prestressed concrete pressure vessel recognized as present in the structural design and one of these limit states refers to crack pressurization.

M. Bender: Could the data, to this date, give any indication as to whether the design methods associated with vessels are conservative or nonconservative. How close are we to resolving uncertainties concerning thermal behavior of concrete?

J. P. Callahan: It is difficult at present to make blanket statements regarding conservatism, or the lack of it in analyses for predicting long-term vessel behavior. In fact, the authors feel that one will never be able to state, unequivocally, that an analysis always gives conservative predictions under all conditions. There are several reasons for this. We must consider: 1. both stress predictions and strain conditions, 2. both thermal loadings and mechanical loadings, and 3. both short-term initial loadings and long-term unloadings and load variation. Analytical predictions which may be conservative with regard to one item in the pairs above might not be conservative with regard to the corresponding item. In other words, analytical predictions can be conservative in one sense and nonconservative in another. The consequence of this is that we can no longer rely on a method being conservative but must strive for methods which are as nearly correct as possible. The program described in the paper is aimed at developing such methods and demonstrating their validity by means of the thermal cylinder test. Since this test has not yet been completed, it will be some time before such information will become available.

ORAL STATEMENT ON THE PAPER BY H. BENZLER AND J. TERPSTRA:
"CONCRETE REACTOR PRESSURE VESSELS - 1969 ASSESSMENT"

by

D. Tytgat, Euratom

On 18, 19 and 20 November 1969 the Commission of the European Communities held a "Second information meeting on work relating to prestressed concrete pressure vessels and their thermal insulation". The article by Benzler and Terpstra recapitulates the findings of this meeting, especially the progress achieved during the last years and the problems still to be solved. Considering the time allocated for this short statement, it will not be possible to provide a complete summary of the contents of the paper, copies of which have been made available to the audience of this conference. I shall therefore limit my remarks to new concepts for prestressed concrete reactor vessels and their thermal insulation which were discussed at the recent Brussels information meeting and which may be of particular interest within the context of this meeting.

Taylor Woodrow Construction Ltd. (United Kingdom) have developed a new technique of circumferential prestressing for the Hartlepool nuclear power plant, which is designed according to the pod boiler system, in which prefabricated boiler units are mounted in penetrations passing vertically through the full height of the vessel walls. For this design, potential tendon paths are severely obstructed by the presence of the boiler pods. It was therefore decided to locate the circumferential prestressing outside the main vessel walls, and to keep the outside surface of the vessel completely free of large penetrations. With these features, it was possible to use a wire-winding technique which had to be specially developed because no system existed which could meet the necessary requirements. The arrangement chosen is based upon the use of concentrated bands of 0.2 in diameter low relaxation wire, wound under tension into channels preformed in the vessel walls. The general layout of the wire-winding

equipment is illustrated in slide 1 (Fig. 6 in paper No. 5). The two vehicles of the machine are carried on a steel platform which can be raised or lowered. Traction is obtained from a chain encircling the vessel. As the machine passes around the vessel, wire is drawn from a spool mounted on the rear vehicle and passes through a tensioning device. The channels for the wire-winding bands are built in precast concrete units which form external shuttering to the vessel.

Philipp Holzmann AG (Germany) have developed a wound multilayer circumferential prestressing system with equidistant wires and intermediate anchors in the form of clamps which draw all the tendons together at several points on the vessel's circumference in a subsequently formed force-locked bond. Four tensioning wires are wound on simultaneously by a machine. Slide 2 (Fig. 9 in paper No. 14) shows an example of the wire-clamping system, and slide 3 (Fig. 23 in paper No. 14) a working model of this tensioning system for a pressure vessel of the AGR type.

The French Commissariat à l'Energie Atomique (CEA), in collaboration with Messrs. Coyne & Bellier of Paris, has worked out proposals for a tensioning system in which the circumferential stresses are taken up by externally located Hoops of high-tensile steel strip. Each hoop consists of a band which is wound round the vessel several times. The prestress is generated by tensioning devices located between the concrete shell and the hoop; slide 4 (Fig. 3 in paper). The efficacy of this system has been proved by tests.

With all prestressed concrete reactor vessels, it is essential that the reactor core be readily accessible from the outside, mainly for the purpose of refuelling. The Socia Company (France) has in this context studied a prestressed concrete vessel with a removable spherical cap, likewise of prestressed concrete, which is attached to the vessel's shell by means of a steel hoop structure; slide 5 (Fig. 4 in paper). The hoop structure is built up of plates and cross-ties and contains large cut-outs

which enable the cap to be attached by means of removable bolts.

Sponsored by the Commission of the European Communities, Fried. Krupp Universalbau (Germany) have developed a multilayer concept for prestressed concrete reactor vessels; slide 6 (Fig. A).

There are four different layers, each of which has a special purpose. The innermost layer - see slide 7 (Fig. B) - is made of a heat -resistant concrete and acts as a thermal shield. The next layer is a ceramic heat insulant.

The third layer is a space limited by an inner and an outer steel liner and filled with water at the same pressure as the reactor coolant. The outer layer is formed by the prestressed-concrete structure and can be kept completely free of temperature gradients. Model tests on a 1:5 scale have been successfully performed.

The performance of thermal insulating systems inside a concrete pressure vessel depends largely on the nature and the physical conditions of the reactor coolant. For CO_2 - cooled reactors, reliable systems have been developed both in France and in the United Kingdom.

Slide 8 shows the "Métalisol" system recently employed by CAFL (France) in the Bugey reactor. It consists of packs of stainless steel cloths suitably spaced by foils and seals and tied together by bolts and a rugged casing. Other designs of a comparable type, however, using mineral fibre instead of metal cloths, are currently being used by Delaney Gallay in several British nuclear power stations of the AGR type.

A thermal shield system basically consists of three components (Fig. 9):

- an insulating layer, which reduces the heat flux from the reactor coolant to the vessel walls;
- a metal plate, usually the leaktight liner, which drains the heat leaking through the insulating layer to the cooling system;

- the above-mentioned cooling system, which evacuates the heat losses to outside reactor vessel.

These components are normally arranged in such a way that the insulation is inside the liner, where it is immersed in the reactor coolant. The cooling circuit is usually located outside the liner, where it is embedded in the concrete. This "conventional" arrangement, shown on the left in Fig. 9, is the only one applied so far in power reactors.

In the arrangement shown in the centre of Fig. 9, the cooling system has moved from the outside to the inside of the liner. The coolant is the same as that of the reactor core and at the same pressure, but at a lower temperature. This system has no special leaktightness requirements and it allows a high degree of prefabrication. It has been developed under a Euratom sponsored contract by Deutsche Babcock and Wilcox and the French firm of Socia.

In the arrangement shown on the right in Fig. 9, both the cooling system and the insulation layer are outside the leaktight liner. In this concept, developed by the French firm of SEEE, the liner is subjected to extremely severe thermal and mechanical stress, which gives rise to extremely tricky problems. On the other hand, it eliminates almost all the problems involved with the other concepts, and it might prove to be an attractive proposition in the case of water-cooled reactors for example.

I hope that this short introduction has given you an impression of the considerable amount of work that has been done in this field in Europe. The utilities in this part of the world have shown complete confidence in the prestressed concrete pressure vessel from the very beginning of commercial nuclear power generation. The best proof of this confidence is certainly the number of prestressed concrete pressure vessels in operation or under construction at the moment - which total 18 in all.

Thank you for your attention.

HTGR FUEL ELEMENT DESIGN,
PERFORMANCE AND EVALUATION

(Session V)

Chairman: R. F. Turner,
Gulf General Atomic

Co-Chairman: J. W. Coobs,
Oak Ridge National Laboratory

HTGR FUEL IRRADIATION PERFORMANCE AND IMPLICATIONS ON FUEL DESIGN*

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ABSTRACT

The fuel element design for large HTGRs is a hexagonal graphite block containing coated particles in bonded fuel rods. The excellent irradiation performance of the multi-layered BISO and TRISO coated particles has been demonstrated in 250 successful tests to burnups up to 59% FIMA, fast fluences up to 8.7×10^{21} n/cm², and temperatures up to 1450°C. Beds of coated particles bonded into fuel rods with carbonaceous matrices have been demonstrated to be stable during 18% FIMA burnup and 6×10^{21} n/cm² fast fluence at 1250°C. Irradiation testing of the reference graphite has shown it to have adequate dimensional and structural stability over the full range of HTGR operating conditions.

INTRODUCTION

The successful development of coated-particle fuels during the past decade has led to simplification of the fuel element design for HTGRs.¹⁻³ Because of the demonstrated excellent stability of the coated particles under the combined conditions of high temperature, high burnup, and high fast fluence, fuel element design has been relieved of the requirements for costly low-permeability graphites and complex fission product purging systems. Fission product control is now achieved mainly by the particle coatings, and the designer has been given much greater freedom to optimize core performance and reduce fuel cycle costs.

*Work supported in part by the U.S. Atomic Energy Commission under Contracts AT(04-3)-633 and AT(04-3)-167, Project Agreement No. 17.

Cores of the Fort St. Vrain 330-MW(e) and the 1100-MW(e) HTGRs are composed of hexagonal-shaped blocks of conventional needle-coke graphite stacked in a close-packed array of vertical columns.^{4,5} This design evolved from the long cylindrical elements used in the Peach Bottom HTGR after it had been proven that fission product control could be achieved by means of the multilayered particle coatings. In the hex-block fuel element, the coated particles are contained in fuel rods located within vertical fuel channels. Coolant channels passing completely through the element are interspaced among the fuel channels.

Results of many irradiation tests have confirmed the excellent performance of HTGR fuel under some of the most severe design operating conditions envisioned for HTGR plants. These results are summarized in this paper, and the design basis for the hex-block fuel element graphite is described.

COATED PARTICLES

Two types of coated particles (BISO and TRISO) have been developed and proved for HTGR use (Fig. 1). Each has a dense, spherical carbide kernel and an inner coating layer of low-density porous pyrolytic carbon (buffer PyC coating). The BISO coating has a single outer layer of high-strength, isotropic PyC, whereas the TRISO coating has a silicon carbide layer sandwiched between inner and outer layers of the high-strength, isotropic PyC. The SiC coating layer provides added retention of metallic fission products. Fuel kernels range in size from 100 to 600 μm in diameter, with overall coating thicknesses of about 130 μm . Mathematical stress analysis models, combined with the results of many irradiation experiments, are used to design the coated particles for specific applications.⁶⁻⁹

Irradiation test results on large numbers of statistically significant samples of BISO and TRISO coated particles have demonstrated their stability to burnups greater than 20% FIMA (fissions per initial metal atom) and to fast fluences greater than $8 \times 10^{21} \text{ n/cm}^2$ ($E > 0.18 \text{ MeV}$) at temperatures of 1300°C. These tests have been conducted for the most part in capsules in the

ETR at NRTS, Idaho, and the GETR at Vallecitos, California. Specimen temperatures are closely controlled and monitored by means of thermocouples located in the center of the fuel samples; fast fluences are determined by analyses of flux wires located throughout the capsules; and burnups are determined by postirradiation isotropic analyses of the fuel. Each coated particle test sample consists of several thousand coated particles, all of which are examined before and after irradiation.

Test results are available to date on about 300 individual samples of coated particles, and over 100 additional samples are currently being tested, including many samples in test elements in the Peach Bottom HTGR.¹⁰ BISO coated particles have survived burnups up to 59% FIMA, fast fluences up to 8.4×10^{21} n/cm², and temperatures up to 1450°C. (A sample is considered to have survived a test if more than 99% of the particle coatings have retained their integrity.) TRISO coated particles have survived burnups up to 27% FIMA, fast fluences up to 8.7×10^{21} n/cm², and temperatures up to 1300°C.

Photomicrographs of BISO and TRISO coated particles tested under conditions more severe than the peak design operating conditions envisioned for HTGR plants are shown in Figs. 2 and 3. The regions of demonstrated successful irradiation performance for BISO and TRISO coated particles with respect to burnup and fast fluence are shown in Fig. 4. These test results include many samples of BISO and TRISO coated particles prepared in full-scale production equipment.

FUEL RODS

The HTGR fuel rods consist of close-packed beds of coated particles bonded together with a carbonaceous matrix. This matrix has been designed for sufficient stability under irradiation to avoid the deleterious effects that have been observed with less stable fuel body matrices. Design features of the fuel rods include use of a graphitizing binder in conjunction with high heat treatment temperatures and radiation-stable graphitic filler materials. A hot injection molding process is used to fabricate the fuel rods. This

process is adaptable to the manufacture of fuel rods up to several inches long. Fuel rods are stacked end-to-end in the fuel channels of the hex-block element to make up the required fuel column height. Typical fuel rods are shown in Fig. 5.

Irradiation tests to 6×10^{21} n/cm² and 18% FIMA at 1250°C have shown that these fuel rods have excellent stability under irradiation. No evidence of coating breakage or of deleterious interaction between the matrix and the particle coatings has been observed. Photographs and photomicrographs of irradiated fuel rods are shown in Figs. 6, 7, and 8.

GRAPHITE FUEL BLOCKS

The active fuel, in the form of bonded rods, is structurally contained and arranged for effective heat transport to the helium coolant within prismatic graphite fuel elements. The fuel element structural graphite also serves as the reactor moderator. Each hexagonal fuel element is 14.2 in. across flats and 31.2 in. in length (see Fig. 9). A standard fuel element contains 210 axially oriented fuel holes, interspaced with 108 coolant channels. Burnable poison for reactivity shimming can be inserted into any of six additional non-fueled holes provided at the corners of the hexagonal element.

The fuel elements are manufactured from a nuclear-purity grade of conventional extruded needle-coke graphite. In Table 1, strength properties of the production fuel element graphite for the Fort St. Vrain Nuclear Generating Station are summarized and compared with values established as design criteria. The longitudinal minimum strength is determined by tensile testing of core drilled specimens taken from the log center, where tensile strength is normally lowest in extruded graphite. One hundred percent of the production logs for fuel element use in the Fort St. Vrain HTGR are being tensile strength tested.

Extruded needle-coke type graphites have been extensively irradiated¹¹⁻¹⁴ to a fast neutron fluence of greater than 8×10^{21} n/cm² ($E > 0.18$ MeV) (the design maximum fast fluence for HTGR fuel elements). Data maps of irradiation

Induced dimensional change versus temperature and fast fluence based upon these results have been constructed for use in fuel element design and performance analyses. These data maps are shown in Figs. 10 and 11 for the axial and transverse directions, respectively. The shaded area on each map envelops the entire range of fluence and temperature experienced by fuel elements within the Fort St. Vrain reactor. Graphite dimensional changes predicted over the full range of design operating conditions are within acceptable values for the overall core and fuel element designs.

In addition to gross fuel element dimensional change design considerations, stresses within the ligaments between fuel rods and coolant channels are evaluated. Ligament stresses occur as a result of the temperature gradients in the direction of heat flow between the active fuel and coolant channels. In addition to the thermal stresses resulting from these gradients, stresses are also induced by differing rates of graphite dimensional change across a ligament because of the temperature dependence of the irradiation-induced graphite contraction rate.

Although the total strain differences resulting from contraction gradients across ligaments are typically large compared with the thermal strain differences (which produce thermal stresses), the contraction gradient stresses remain relatively low because of irradiation-induced creep.^{15,16} Figure 12 shows the lifetime stress history for a Fort St. Vrain fuel element at the location within the graphite ligament experiencing maximum axial tensile stress. The maximum tensile operating stresses, after irradiation-induced creep relaxation of the initial thermal stresses, typically occur adjacent to the fuel hole. Stresses at the coolant channel boundaries are typically compressive. As shown in Fig. 12, the thermal stress distribution becomes additive to the contraction-induced stress distribution when the reactor is shut down and the operating temperature gradient is removed. Figure 13 shows the axial stress distribution within a Fort St. Vrain fuel element ligament during full-power operation and at the time in fuel life of maximum contraction-induced stress. Stresses in the transverse (in-plane) direction are typically smaller than the axial stresses. All of these stresses are well below the measured tensile strengths of the production graphite used for the fuel blocks.

SUMMARY

Extensive irradiation tests have confirmed the excellent irradiation performance of HTGR fuel element materials and components under combined conditions of high temperature, high burnup, and high fast fluence. This demonstrated performance of the fuel under the most severe design operating conditions envisioned for HTGR plants has reaffirmed the basic fuel element design concept of the hexagonal graphite block containing coated particles in bonded fuel rods. This fuel element is simple, develops no high stresses during operation, and promises to be economic to fabricate and reprocess.

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Table 1. Properties of Production Graphite Utilized
for the Fort St. Vrain Fuel Blocks

	Design Criteria	Average Measured Value
Density, g/cm	1.70 min	1.77
Tensile strength, psi		
Longitudinal mean	1500	1920
Longitudinal minimum	1000	1690 ^a
Transverse mean	1000	1160
Transverse minimum	600	1010
Compressive strength, psi		
Longitudinal	3000	4400
Transverse	3000	4700

^aBased on 100% testing of 752 logs; the σ value is 360 psi.

Fig. 1. BISO- and TRISO-Coated Particles.

M275-48(P18)

(200X)

Fig. 2. BISO-Coated Particle after Irradiation to 22% FIMA Burnup and 8.4×10^{21} n/cm² Fast Fluence at 1300° C.

M251-94(P20)

(175X)

Fig. 3. TRISO-Coated Particle after Irradiation to 27% FIMA Burnup and 8.7×10^{21} n/cm² Fast Fluence at 1300° C.

Fig. 4. Regions of Demonstrated Irradiation Performance for BISO- and TRISO-Coated Particles.

Fig. 5. HTGR Fuel Rods; These Rods are 1/2 in. in Diameter and 3/4 in. Long.

K340-39

(~2X)

K340-65

(2X)

Fig. 6. Fuel Rods after Irradiation to 5.0 (Left) and 5.7 (Right) $\times 10^{21}$ n/cm² Fast Fluence and 18% FIMA Burnup at 1250° C. The ends of severed thermocouple tubes project from the ends of the rods. These rods are 1/2 in. in diameter and 3/4 in. long.

M340-148

(125X)

M340-125

(75X)

Fig. 7. Cross Sections of Fuel Rods after Irradiation to 5.0 (Left) and 5.7 (Right) $\times 10^{21}$ n/cm² Fast Fluence and 18% FIMA Burnup at 1250° C. No deleterious effects were noted in the coated particles in these fuel rods.

M340-146

(250X)

M340-134

(250X)

Fig. 8. Enlarged Views of the Graphitic Matrices of the Same Two Irradiated Fuel Rods that Are Shown in Fig. 7.

Fig. 9. Standard Hex-Block Fuel Element for Fort St. Vrain and Large HTGRs.

Fig. 10. Irradiation-Induced Dimensional Change Contours for Fuel Element Needle-Coke Graphite (Axial Direction). Shaded area defines envelope of operating conditions.

Fig. 11. Irradiation-Induced Dimensional Change Contours for Fuel Element Needle-Coke Graphite (Transverse Direction). Shaded area defines envelope of operating conditions.

Fig. 12. Maximum Local Axial Tensile Stress in Fuel Element Graphite for the Fort St. Vrain HTGR. The measured mean tensile strength of the graphite is 1900 psi.

Fig. 13. Axial Tensile Stress Distribution in Fuel Element Graphite Ligament (Fort St. Vrain HTGR).

DISCUSSION

H. Krämer: The two types of particles, the uranium and thorium, are mixed in the fuel rods, and therefore, have the same lifetime. Do you expect difficulties from this with regard to temperature changes and fissionable material depletion (high excess reactivity)?

W. V. Goeddel: No. This factor has been covered in our irradiation test program. The irradiated fuel rods shown in the paper contained both types of particles and were tested under thermal gradients more severe than those in an HTGR. As reported, no difficulties were encountered.

K. Wirtz: Who is the manufacturer of your graphite?

W. Goeddel: Great Lakes Carbon Corporation is supplying the graphite for the Fort St. Vrain HTGR.

K. Wirtz: What is the clearance between your hexagonal blocks; do you expect lateral dimensional changes?

W. Goeddel: The clearance between adjacent fuel element columns is about 1/8 inch. There will be some radiation-induced shrinkage of the blocks in the radial direction, ranging up to a maximum of 1%. The extensive irradiation data on this type of needle coke graphite indicates that no expansion of the blocks will occur within the design lifetime of the fuel elements in the HTGR.

R. D. Vaughan: Which feature of the particle or block design currently sets a limit on the core power rating you achieve in your reactor?

W. Goeddel: Fuel centerline temperatures.

G. Meijer: Referring to the stresses calculated for the Fort St. Vrain fuel block, can you tell us whether you analyzed the stresses for cases like a new fuel block at the core boundary and like inserting control rods in a fuel block, both giving rise to steep temperature gradients. Can you also tell in what part of the core and for what conditions the highest stresses are experienced?

W. Goeddel: We have analyzed the stresses for these and many other conditions. The highest stresses arise in new fuel near the core boundary.

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6/7/68

ABSTRACT

Fuel elements proposed for advanced HTGR's consist of hexagonal blocks of graphite with parallel fuel and coolant channels. The fuel channels are loaded with pyrolytic-carbon-coated particles of fuel and fertile materials. We developed and demonstrated a method of bonding the coated particles into "fuel sticks" that will keep the fuel bed from falling apart during handling or during accidents that might damage the graphite core blocks. The bonded fuel sticks also facilitate inspection of the fuel loading and enhance the transfer of heat from the fuel to the coolant.

Several problems were overcome in developing a satisfactory binder. When a bed of particles was impregnated with phenolic resin binder and then heated to carbonize the resin, shrinkage broke coatings on some particles. Modified coatings that survived carbonization produced weak fuel sticks that disintegrated during irradiation to HTGR fast-neutron fluences. By using high concentrations of filler material in the binder (40 wt % Poco graphite powder in pitch or 29 wt % Poco graphite plus 29% Thermax carbon black in either pitch or resin) we produced fuel sticks that remained intact throughout irradiation. Only one damaged coating was found among 2000 coated particles examined in irradiated sticks prepared with these binders.

INTRODUCTION

Fuel elements containing pyrolytic-carbon-coated (U,Th)O₂ or (U,Th)C₂ microspheres have found important application in high-temperature gas-cooled reactors such as the Dragon Reactor in England, the AVR in

*Research sponsored by the U.S. Atomic Energy Commission under contract with the Union Carbide Corporation.

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Germany, and the Peach Bottom, UHTREX, and Fort St. Vrain Reactors in the United States. The core for the Fort St. Vrain Reactor is composed of hexagonal graphite fuel elements stacked in vertical columns.¹ The fuel will be located in 1/2-in.-diam holes that are parallel to the vertical coolant holes in the element. Core designs for large HTGR's are expected to be based on the Fort St. Vrain fuel element. To facilitate inspection of the fuel loading and to avoid spilling fuel in case of damage to a fuel element, it is proposed that the mixture of coated fissile and fertile particles be bonded together with a carbonaceous matrix before being inserted into the fuel element. Our objective, therefore, was to develop a bonded fuel body that would withstand irradiation to peak HTGR exposures without particle failure or severe debonding. Typical exposures for large HTGR's are 20% burnup and a fast-neutron (> 0.18 Mev) fluence up to 8×10^{21} neutrons/cm² at temperatures of 600 to 1300°C.

Desiring a simple fabrication technique, we first developed a method for fabricating such "fuel sticks" by mixing Asbury graphite powder with a phenolic resin and injecting the resulting "grease" into a mold loaded with loose coated particles. So that the grease would be fluid enough to penetrate a 15-in.-long bed of particles, the mix contained only 15 to 20 wt % graphite in resin.² After the thermosetting resin had hardened at 80°C, it was carbonized at 1000°C. The final matrix density of such bodies was about 0.6 g/cm³. Shrinkage was a problem even in the carbonizing step. The resin adhered so firmly to coatings deposited from propylene that some coatings cracked during carbonizing. To prevent this, we developed a sacrificial coating consisting of an anisotropic sealer layer and a porous outer layer. This prevented the coating from cracking during carbonizing, but weakened the bonded bed.

Bonded beds of this type were first tested in irradiation experiments in the ORR. In two separate experiments,^{3,4} bonded beds of coated particles survived to high burnup ($> 25\%$ FIMA), and the matrix was relatively undamaged by the low fast-neutron fluences ($< 10^{21}$ neutrons/cm²). More recently, two tests in HFIR target capsules showed that severe

degradation of bonded beds can occur during irradiation to full design fast fluences. The first of these HFIR experiments, HT-1, contained inert* particles that were coated with two-layer carbon coatings plus the sacrificial and nonbonding layers and bonded with the same matrix material that survived in the ORR experiments.⁵ Fast-neutron damage to such low-density matrices results in considerable densification and shrinkage before other effects become evident. In this experiment, a linear shrinkage of 10% was observed on samples of the matrix material exposed at 1000°C at a fast-neutron (> 0.18 Mev) fluence of 7.5×10^{21} neutrons/cm², and the matrix of bonded beds deteriorated so extensively that the beds were no longer bonded at the end of the test, as seen in Fig. 1. Some particle coatings were broken also, as shown in Fig. 2.

*Inert kernels are required in HFIR because the gamma heating and thermal flux are so high that the bonded beds would be too hot if fissioning occurred in the bed. In HT-1 and -3 experiments the inert particles consisted of ZrO₂.

Fig. 1. Loose Coated Particles and Fragments of Matrix Material After Irradiation to a Fluence of 7.5×10^{21} neutrons/cm² at 1000°C.

Fig. 2. Polished Section Showing Typical Appearance of Coated ZrO_2 Particles Bonded with Resin Containing Asbury Graphite Flour. (a) Unirradiated. (b) After irradiation at 800°C to a fast fluence of 6×10^{21} neutrons/ cm^2 ; about 20% of the coated particles were broken in two such specimens. As polished. 100X. Reduced 16%.

In a second irradiation experiment, HT-3, the sacrificial coating concept was abandoned. The shrinkage of the matrix during carbonizing was reduced somewhat by increasing the content of flake graphite from 15 to 25%. This series of samples also failed to survive irradiation to 8.0×10^{21} neutrons/cm² (> 0.18 Mev) at 1000°C, as shown in Fig. 3. The particle beds completely debonded and many particle coatings were broken.⁶

DEVELOPMENT AND FABRICATION OF IMPROVED BONDED BEDS

We then sought to develop a bonded bed having the required irradiation stability without regard for ease of fabrication. Two obvious ways to improve irradiation performance were: (1) to select binders that had high coking values and that yield graphitizable coke, and (2) to add large amounts of graphite or other filler to the binder. Such mixtures are much more viscous than the binders containing only 15 to 25% filler; higher pressures are thus required in order to infiltrate the bed of coated particles.

To facilitate fabrication of bonded beds, we designed a mold to keep the particle bed from expanding and resulting in a fuel stick with less than the required 62-65 vol % particle loading. The bonding material or "grease" was forced through the particle bed by the use of a specially fabricated pressure device or "greasegun," which is shown in Fig. 4. The threaded nipple was attached to the mold containing the particles, and pressure was applied by driving the double O-ring sealed piston. Pressure was applied until excess bonding material was forced out at the exit end of the mold. When preheating was required with thermoplastic binders the neoprene O-rings were replaced with Teflon O-rings.

Solidification of the bonding material was achieved either by polymerization of thermosetting binders at 80°C, or by cooling to room temperature when using pitch binder, which is thermoplastic. After the fuel sticks were hardened by polymerization or cooling, they were carbonized by heating to 1000°C in an inert atmosphere. The fuel sticks

Fig. 3. Irradiated Bonded-Bed Samples Showing Extent of Debonding After Testing at 1050°C to Fluence of 8×10^{21} Neutrons/cm² in Experiment HT-3. (a) Loose particles and fragments of matrix material from bonded bed prepared at ORNL. (b) Loose particles and fragments of coatings and matrix material from bonded specimen containing TRISO-coated particles prepared at GGA.

must be heated slowly during carbonization to avoid cracking or distortion by gases released from the binder. We found that heating at a linear heating rate to reach 1000°C in 24 hr was satisfactory. Cooling was accomplished in about 12 hr by simply turning off the power to the furnace.

In our efforts to improve the performance of bonded beds, we initially selected Poco graphite flour* as filler because of its remarkable irradiation stability.⁷ We found that we could inject a mix consisting of 40 wt % Poco graphite flour ($-40\ \mu$) in thermosetting binders such as Varcum 8251† or phenolic resin.‡ Polymerization of the thermosetting binders was catalyzed by the addition of 5 wt % (based on binder weight) maleic anhydride to the Varcum, and 10 wt % to the phenolic resin. After carbonizing at 1000°C, we found the matrix carbon density to be 0.9 g/cm³ compared with 0.6 g/cm³ for the matrix made previously. We found also that a similar body could be made using thermoplastic binders such as coal tar pitch.§ With thermoplastic binders the mix is made fluid by preheating all materials and hardware before injection. With grade 15V pitch the preheating temperature was at least 150°C. To raise the carbon content further we added Thermax|| to both the thermosetting and thermoplastic mixes. A mix containing 29 wt % Poco graphite and 29 wt % Thermax carbon black in Varcum gave a carbonized matrix density of 1.25 g/cm³. A similar mix was injected with grade 15V pitch as the binder. Fuel sticks at least 2 in. long could be made with all of these mixes.

*Grade AXM graphite from Poco Carbon Company.

†Furfuryl alcohol from Varcum Chemical Company.

‡P-514 Cement Primer from Great Lakes Carbon Company.

§Grade 15V Pitch from Allied Chemical Company.

||A spherical carbon black from R. T. Vanderbilt Company.

IRRADIATION TESTING OF IMPROVED BONDED BEDS

We have completed an irradiation test, HT-4, of the improved bonded beds in the target region of HFIR. For the experiment we used injection-bonded particle beds with either grade 15V pitch or Varcum as the binder. Filler materials in the pitch binder were either 40 wt % Poco graphite or 29 wt % Poco graphite plus 29 wt % Thermax carbon black. Only the 29 wt % Poco graphite and 29 wt % Thermax combination was used with the Varcum binder.

In this experiment we used inert particles that consisted of carbon kernels coated with monolayer coatings deposited from propylene at 1250°C. The coated particles were annealed at 2100°C before bonding to increase the irradiation stability of the coatings and to minimize stresses set up by differential thermal expansion of the carbon kernels and monolayer coatings. The composition and heat treatment of the various specimens are given in Table 1.

Capsule Assembly

The capsule assembly consisted of a singly contained uninstrumented capsule with exterior shape and dimensions basically that of a typical target rod. Graphite magazines containing the bonded bed and loose particle specimens were loaded into the central 20-in. length of the capsule. The arrangement of the magazines and their respective temperatures are shown in Fig. 5. The two center magazines CP-14 and -15 that held bonded beds and the loose particle magazine M-4 were designed to operate at 1065°C. The remaining bonded bed and loose coated particle magazines were designed to operate at 800°C. The capsule assembly was irradiated for three cycles in the HFIR target region. The fluences obtained in two previous tests HT-1 and -3 irradiated for the same period of time and in the same target position are shown in Fig. 6.

Postirradiation Evaluation

After irradiation and disassembly, all bonded beds were intact and could be handled in-cell. The typical postirradiation appearance is illustrated by the group of specimens shown in Fig. 7 that were irradiated

Table 1. Bonded Particle Specimens for HT-4 Experiment

Specimen	Materials		Heat Treatment ^a Temperature (°C)	Magazine
	Binder	Filler		
JF78-1	(b)	(c)	2100	CP-15
-2	(b)	(c)	2100	-15
-4	(b)	(c)	2100	-16
-5	(b)	(c)	2100	-16
-8	(b)	(c)	1500	-13
-9	(b)	(c)	1500	-14
-10	(b)	(c)	1500	-15
-11	(b)	(c)	1500	-16
-21	(d)	(c)	1500	-13
-22	(d)	(c)	1500	-13
-23	(d)	(c)	1500	-14
-24	(d)	(c)	1500	-14
-14	(d)	(c)	2100	-15
-16	(d)	(c)	2100	-15
-19	(d)	(c)	2100	-16
-20	(d)	(c)	2100	-16
-25	(d)	(e)	2100	-13
-27	(d)	(e)	2100	-13
-28	(d)	(e)	2100	-14
-29	(d)	(e)	2100	-14

^aHeated in argon for 30 min.

^bVarcum 8251 (prepolymerized furfuryl alcohol from Varcum Chemical Company).

^c29 wt % Poco AXM (-40- μ -size graphite flour from Poco Carbon Company) plus 29 wt % Thermax (a soft spherical carbon black from R. T. Vanderbilt Company).

^dGrade 15V pitch (coal tar pitch from Allied Chemical Company).

^e40 wt % Poco AXM (-40- μ -size graphite flour from Poco Carbon Company).

at 800°C to a fluence as great as 6×10^{21} neutrons/cm². The average dimensional changes that occurred at the low and high irradiation temperatures were -1.4 and -2.62%, respectively. At the low irradiation temperature the shrinkage increased with increasing fluence; at the high irradiation temperature the shrinkage seemed to decrease with increasing fluence. At either temperature the shrinkage did not seem to be influenced by the composition of the binder phase or preirradiation heat treatment.

Fig. 4. Injection Device for Particle Bonding Material.

Fig. 6. Fluences Obtained in Two Previous HFIR Target Tests HT-1 and -3. The data represent the fluence obtained in HT-4. The location of the various HT-4 magazines are shown at the bottom.

Fig. 7. Appearance of Bonded Beds of Coated Particles After Irradiation in Capsule HT-4 to Fluence of 6×10^{21} neutrons/cm² at 800°C.

We crushed about 25 particles from each of the loose-particle samples irradiated at the low and high temperatures, and measured the densities of the coating fragments in a density gradient column. We found that the densities increased 4 and 7% at the low and high temperatures, respectively. The linear shrinkage rates corresponding to these density changes would be about -1 and -2%, which is in agreement with observed shrinkage rates for the bonded beds. Thus the shrinkage of the bonded beds seems to be controlled by the behavior of the close-packed particles.

Metallography of two specimens with the higher loading of carbon filler in the matrix (29 wt % Poco graphite powder and 29 wt % Thermax carbon mixed with Varcum or grade 15V pitch) revealed only one damaged coating of more than 1200 coated particles examined in the two specimens. Both of these specimens were irradiated at the higher temperature (approx 1050°C). The matrix of the Varcum-bonded specimen showed extensive fine cracks that had widened and extended, but it was otherwise similar to the unirradiated control samples as shown in Fig. 8. The matrix was also well bonded to the particle coatings. The pitch-bonded matrix, on the other hand, showed fewer cracks and did not bond tightly to the particle coatings. Comparison of the pre- and postirradiation microstructures in Fig. 9 shows that the pitch-bonded body is relatively unaffected by the high-temperature irradiation. Metallography of two identical specimens irradiated at 800°C did not reveal any broken coatings and confirmed the observations cited above. Thus only one damaged coating was observed out of more than 2000 coated particles examined in four specimens. Metallographic examination of two specimens of the third type (bonded with 40% Poco AXM graphite in pitch) is in progress, but evidence from the macroscopic and dimensional examinations indicates that little or no damage occurred in these specimens either.

DISCUSSION AND CONCLUSIONS

Although the coated particles in the HT-4 experiment had monolayer coatings deposited over carbon microspheres, we believe that they are basically like those two-layer coatings (porous inner-LTI outer) that

R-50107

Fig. 8. Microstructures of Bonded Beds with Varcum Binder Tested in HFIR HT-4. The filler consists of 29 wt % Poco AXM graphite flour and 29 wt % Thermax carbon black. The particles are 220- μ -diam carbon microspheres coated with a 75- μ -thick LTI monolayer coating. (a) Unirradiated, and (b) irradiated at 1050°C to 8×10^{21} neutrons/cm². As polished, 100 $\times$. Reduced 18%.

R-50 106

Fig. 9. Microstructures of Bonded Beds with Pitch Binder Tested in HFIR HT-4. The filler consists of 29 wt % Poco AXM graphite flour and 29 wt % Thermax carbon black. The particles are 220- μ -diam carbon microspheres coated with a 75- μ -thick LTI monolayer coating. (a) Unirradiated, and (b) irradiated at 1050°C to 8×10^{21} neutrons/cm². As polished, 100X. Reduced 22.5%.

failed in the two earlier HFIR target experiments (HT-1 and -3). The carbon microspheres used as kernels were of low density and shrank extensively during irradiation, and this shrinkage left the monolayer coating unrestrained. We believe the improvement stems from the new matrix compositions, which have lower shrinkage rates and do not bond so tightly to the particle coatings.

Thus the results of the HT-4 experiment demonstrate that a bonded bed can be fabricated which will withstand the peak temperature and fluence of an HTGR. The samples, however, were made with an expensive new material, Poco graphite flour, and were only 0.5 in. long. Additional work will be required to develop longer bodies with equivalent structures using cheaper raw materials and automated fabrication processes.

It should be noted that the bonded beds tested here did not contain fuel or coatings with SiC barrier layers. Also, the neutron dose rates are much higher than in an operating HTGR. Additional irradiations will be required to assess the effects of these variables.

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DISCUSSION

C. B. von der Decken: It was mentioned that the bonding will be done inside the fuel element block and that some cracking may occur. Has such an arrangement been irradiated up to now?

J. L. Scott: No.

L. W. Graham: What were the dimensional changes observed in the Poco-type bonded beds and how significant are these with respect to the fuel operating temperature in a power reactor?

J. L. Scott: The beds which were exposed to the highest fluence and temperature showed a linear shrinkage of 2.8 percent. The maximum fission rate occurs with a fresh fuel block and the power density decreases as the exposure increases. By the time the dimensional changes become significant, the heat generation rates are enough lower that the temperature is also lower in spite of the higher thermal resistance.

M. Dalle Donne: If I have rightly understood, the advantage of using a bonded bed is to increase the thermal conductivity. However, you have large cracking in the matrix so that the increase in conductivity could be lower than what you would expect. Do you not find that a loose bed, with a third particle of smaller diameter (100 micron or so) together with the other two envisioned, could have roughly the same conductivity with the advantage of having a higher fuel density?

J. L. Scott: The principal reason for using bonded beds is for safety, but there are heat transfer advantages as well. Even though the matrices are badly cracked, we expect the bed conductivity to be two to four times higher than a bed of loose particles. For a given power density the thermal gradient across a bed of particles would be one-half to one-fourth that of a loose bed. This would decrease the change of the Amoeba effect.

The particle diameters in present HTGR designs are 600 microns and 400 microns for the fertile and fissile particles, respectively. To markedly increase the particle density, a third particle would have to be less than 40 microns in diameter. This is below the limit of a usable coated fuel particle.

D. D. Tytgat: 1. To what extent has the Oak Ridge National Laboratory Programme on bonded coated particles included the materials used by Gulf General Atomic? 2. If the separation of fertile and fissile materials in Gulf General Atomic fuel elements is obtained by the presence of a SiC layer during the burning reprocessing operation, and not by a particle size effect, why not adopt the same diameter for both particles? This would help the loading of fuel elements.

J. L. Scott: 1. Both Oak Ridge National Laboratory and Gulf General Atomic have tested bonded beds with natural graphite fillers and both use pitch binders. Gulf General Atomic has not tested Poco graphite because of its high cost. 2. Because of much higher fission density and burnups in fissile particles, it is necessary that they be smaller than the fertile particles for heat transfer reasons.

J. Bugl: So far irradiation testing was performed at 1050°C. Do you foresee irradiation at temperatures above 1050°C?

J. L. Scott: Because of the high thermal flux in the HFIR reactor, fissile material burns-out quickly. In order to avoid large temperature changes, we depend primarily on gamma heating for our tests. When gamma heat alone is used, a convenient maximum temperature is 1050°C. We could achieve high temperatures if we added a fissile plus a fertile material combination which would yield a constant fissioning rate.

THE DESIGN OF PRISMATIC HIGH TEMPERATURE REACTOR FUEL ELEMENTS

E. Smith
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ABSTRACT

In this report four designs of prismatic fuel pin suitable for use in HTR power reactors are described and their relative thermal performance in a fully and continuously gagged core with a gas outlet temperature of 800°C is compared.

The stresses arising in fuel pins are described and the nature of the change in these stresses during irradiation is discussed. Attention is drawn to the importance of the material properties and their variation with irradiation dose and particularly to the need for defining the properties of the fuel matrix.

INTRODUCTION

Present power reactor designs based on the HTR concept and using prismatic fuel elements envisage a core construction consisting of stacked moderator blocks incorporating more or less evenly spaced coolant channels within which the fuelled pins are located and supported. The moderator blocks, together with their charges of fuel pins, are designed to be handled into and out of the core by the fuel handling machinery and this operation may be carried out either on-load or off-load according to the detailed design of the reactor. As a result it is clear that in this concept the residence time of the moderator in the core is the same as that of the fuel. The maximum cross sectional dimensions of the block are largely dictated by the size of available material of the preferred specification, but may be modified by handling requirements and the ultimate choice of coolant channel dimensions. Cross sectional shape, and preferred pitch or spacing of the channels taken together with considerations of the likely influence of carbon to uranium ratio within the core further influence the overall design which must also take account of the allowable inter-channel ligament dimensions having due regard to

manufacturing tolerances and operational stress limitations. Within the coolant channels the fuel pin design limits are largely thermal, clearly that design which results in the lowest peak fuel temperature and temperature gradient for a given core pressure drop must be the desirable target, for on the one hand core power densities are maximised and on the other overall reactor costs are minimised. The final design optimum will, however, depend on the relative effects of core power density and core pressure drop on overall generating costs and the inter-play of limitations such as the minimum desirable thickness of graphite sheaths and fuel bed. It is the purpose of this report to examine the relative merits of various fuel pin designs against such a background.

DESCRIPTION OF FUEL PIN DESIGNS

General Description

Currently three basic forms of prismatic fuel pin designs may be identified. They are the hollow rod, the tubular non-interacting rod and the tubular interacting rod designs. The hollow rod is cooled at its exterior surface only while all tubular designs are cooled both internally and externally. The tubular non-interacting rod concept is further defined in two designs; one with solid cylindrical fuel compacts incorporated into a tubular teledial pin, whilst the second makes use of unrestrained annular compacts within the double wall of a tubular pin. These designs are illustrated notionally in Fig. 1. As the names imply, the interacting design allows the fuel body to interact with one of its containing graphite walls whilst in the non-interacting design this restraint is avoided either by incorporating sufficiently large gaps between the fuel and its containing walls or by an inherent feature of the design itself as in the teledial and hollow rod concepts.

The existing preferred proposals for all these designs incorporate three or four equally spaced full length ribs integral with the exterior surface for centralisation and location of the fuel pins within the coolant channel. Axial alignment of the ribs of adjacent

fuel pins may be achieved by recessing one of the ribs into a suitably machined slot in the coolant channel wall or by a keying feature between mating end caps or tubes. Alternative proposals exist and are being considered in which centralisation of the fuel pin in the channel is maintained only at the extremities of the pin, the external ribs are omitted and bowing is allowed to occur freely with the object of reducing those stresses associated with the restraint of the fuel pins by their interaction with the walls of the channel. Theoretically such a proposal is attractive since it can be shown that the thermal and irradiation induced effects lead ultimately to a stable configuration of the pin within the channel. However peak fuel temperatures may be adversely affected and the attainment of the prime objective (absence of bowing stresses) will depend ultimately on the degree of bowing to be expected in the most adverse cases of cross pin fast neutron flux and thermal gradients and the pin/channel radial clearance. In all designs it is the Project's recommendation that the average outer graphite wall thickness should not be less than 5 mm to provide adequate strength and robustness after taking due account of likely corrosion effects and to provide an additional significant delay and hold-up of fission products in the unlikely event of fuel failure.

A slightly thinner wall can be accepted for the internal coolant passage of the tubular designs and here the recommendation is a minimum thickness of 4 mm. The ligaments between fuel holes in the teledial can be thinner still, the minimum thickness probably being dictated by graphite machining limitations.

Tubular designs may require mating features to limit cross channel coolant flow, this feature takes the form of a simple free fit spigotted joint between end caps which also serves to maintain alignment of the central channel.

The Hollow Rod

The hollow rod design consists of a hollow cylindrical graphite rod, closed at both ends, and housing a number of annular fuel bodies totally unrestrained at their inner radius, (i.e., no internal graphite

wall). In this design the graphite tube may have one closed end integral with the tube and one separate end cap or it could have two separate end caps. (See Fig. 2.) There are no fuel/graphite interactions in this design but increases in the external fuel/graphite sheath gap and decreasing thermal conductivity with irradiation result in peak fuel temperatures at about $\frac{1}{4}$ of the residence time of the rod in the core. It will be shown later that, even without taking this effect into account, the thermal performance of the hollow rod design is inferior to that of the tubular designs. The central void in this design also provides a low resistance path for parasitic coolant flow which will occur in some measure due to the permeable character of the graphite walls and may increase if the end cap seal becomes defective. Such flow, occurring over the hottest face of the fuel body, may enhance corrosion of this region; its likely effects on fuel behaviour must therefore receive careful consideration.

The Tubular-Interacting Rod

In this design the outer tubular graphite sheath is similar to that of the hollow rod design and a second separate inner graphite sleeve designed to "float" between the end fittings is added. As in the hollow rod design one end feature may be integral with the outer graphite tube or both end caps may be separate components (see Fig. 3). The initial gaps between fuel body and graphite walls are only sufficient to allow for manufacturing tolerances and non-interaction at either face in going from cold to peak operating temperatures, subsequent shrinkage of the fuel relative to the graphite wall is allowed to produce stresses within the fuel matrix and the internal sheath. Clearly in this concept interface gap effects are minimised at the expense of increased fuel and inner graphite tube stresses. It will therefore be necessary to demonstrate that neither the fuel nor the fuel sleeve "fail" due to this interaction before such a proposal can be fully acceptable. It must also be observed that the interaction stresses will be at their maximum if a coolant failure incident should coincide with that period of irradiation in which the equilibrium interaction stresses are at a peak value (see Section 4).

The Tubular Non-Interacting Rod (Annular Fuel)

The non-interacting fuel rod design incorporating hollow cylindrical fuel compacts is very similar to the interacting design. In this design inner and outer graphite walls and one end feature may be integral and one separate end cap is provided (see Fig. 4). It will be shown later that this design has a thermal performance intermediate between that of the hollow rod and that of the tubular interacting design. Design aspects requiring careful attention in this concept are:

- (i) that the initial gaps must be computed correctly to avoid interactions without too high a thermal penalty, this requires reliable shrinkage data for the graphite and fuel body over the whole temperature and dose range.
- (ii) that, since the heat division between inner and outer channels changes continuously with time, it is necessary to examine in detail, the thermal, mechanical (stress), and graphite corrosion performance of the design over the full lifetime of the fuel rod in the reactor core.

The Tubular Teledial Rod

The teledial fuel rod design has all the thermal benefits associated with the use of both inner and outer cooling, the fuel does not interact with its containing graphite walls and gap growth has only a limited effect on thermal performance due to the smaller dimensions of the fuel body. Basically the design consists of a thick walled cylinder with evenly spaced fuelled holes within the wall. The fuelled holes, which may be drilled from both ends of the cylinder, are closed at both ends by graphite end caps. (See Fig. 5.)

In contrast to the annular fuel non-interacting design, inner and outer channel performance is not affected by irradiation shrinkage. By comparison with the other fuel pin designs thermal computation is more complicated, strictly requiring a two-dimensional analysis combined with axial (channel) integration, though the present evidence from limited electrical analogue experiments and two-dimensional

calculations shows that a suitable annular model can be chosen which predicts peak fuel temperatures agreeing within about 10°C . Manufacturing costs for this design are also expected to be somewhat higher and for the same fuel space the flow area in a given channel is smaller than that of the other tubular designs resulting in a relatively greater core pressure drop. It will be shown later that the thermal performance of the teledial is only slightly inferior to that of the tubular interacting design, better than that of the tubular non-interacting design and markedly superior to that of the hollow rod design. Careful consideration must be given in this concept to the stresses arising in the rod due to the presence of connecting ligaments between the inner and outer graphite walls.

RELATIVE THERMAL PERFORMANCE OF FUEL PINS

Comparison in Fixed Channel Geometry

For a rigorous and true comparison of the relative merits of the various HTR fuel pin designs described in the previous paragraphs it would be necessary to optimise the physical and thermal design of a power reactor for each proposal using identical limitations with regard to maximum fuel and graphite surface temperatures (taking proper account of time-temperature history) and ultimately comparing the overall generating cost of each design. For obvious reasons this method of comparison cannot be pursued in this report. However, it is considered that the relative thermal performance of each design can be illustrated as follows: a series of reactor designs is assumed, in each of which the core is fully and continuously gagged to give a channel gas exit temperature of 800°C for a coolant gas inlet temperature of 300°C . A fixed channel diameter of 68 mm with a corresponding equivalent channel pitch for the core as a whole of 95 mm, and an active core height to diameter ratio of 0.655 was assumed throughout. The dimensions of channel diameter and pitch, together with the height to diameter ratio of the core are used to determine the active

core height (H) for each case from the expression:

$$H^2 = \frac{2\sqrt{3}}{\pi} \cdot \frac{P_r}{P_c} \left(R_{hd} \cdot p \right)^2$$

where:

P_r = Total reactor thermal power (1,500 MW)

P_c = Mean channel power (MW)

R_{hd} = Height to diameter ratio (0.655)

p = Mean channel pitch

Identical heat transfer correlations and material properties were used in making the analysis which further assumed a channel axial peak to mean power of 1.20, and a maximum radial power factor of 1.20 for all designs, but initial age factors of 1.15, 1.20 and 1.23 for the hollow rod, tubular (non-interacting and interacting) rod and the teledial respectively. The basis for the fuel space design was a hollow rod fuel compact with 42 mm O.D. and 25.4 mm I.D., the cross sectional fuel area in the hollow rod and tubular designs were then assumed to be equal and constant. Some departure from this constant value of fuel space was necessary for the first (9-hole) teledial design analysis in this report for which a fuelled area equal to 0.804 of that of the hollow rod was assumed. For constant heavy metal density this has the effect of increasing the carbon to uranium atom ratio in otherwise identical cores and results in the higher age factors already assumed. Alternatively, to obtain similar carbon to uranium atom ratios and hence age factor in the teledial element either the heavy metal density in the fuel or the fuelled cross section could be increased by about 20%. The more general effects on the physics of the core and ultimately on any associated cost penalty are amenable to calculation. Typical N_c/N_u ratios for the designs discussed are 230, 240 and 295 for the hollow rod, tubular rods with annular fuels, and the teledial respectively.

The results of this series of calculations, which were made only for 'start of life conditions' and therefore did not take account of worsening thermal performance due to gap development, are presented in the graphs of Figs. 6 to 9 which show the characteristics of the various designs for a fixed channel size of 68 mm, the independent variable in these curves is the peak channel power. The limiting fuel and surface temperatures of 1225°C and 1000°C chosen in this comparison were based on the expectation that peak random temperatures would be about 100°C to 150°C and 40°C to 80°C greater than the peak nominal values for fuel and graphite surface respectively. It should perhaps be observed that random effects on fuel temperature, due for example to fuel/graphite sleeve gaps and eccentricity, are likely to be at their greatest in the hollow rod and tubular non-interacting designs since the smaller dimensions of the teledial and built in restraint of the tubular (interacting) design limit these effects. It will be seen from the graphs that, for the 68 mm channel fuel temperature is the limiting feature of all designs. At this limit the core power density, core pressure drop, peak channel power and graphite surface temperatures for the various designs in a 68 mm channel are compared in Table 1.

Comparison in Typical Power Reactor Geometry

Following upon the general survey discussed above, a thermal analysis was made taking into account dimensional and property changes during life for specific designs of hollow rod, tubular interacting and non-interacting, and teledial (in this case an 8-hole design) fuel pins in a typical power reactor. For all designs continuous on-load gagging in a downflow core at a coolant pressure of 55 atm was assumed. Best estimate nominal peak fuel temperatures throughout life were calculated and systematic and random effects, computed separately, were added to the nominal values to obtain Peak systematic and Peak Random temperatures. The systematic effects taken into account included coolant leakage, heat leakage, fuel eccentricity, gag adjustment error and pin power tilt, while the random effects allowed for two standard deviations in fuel thickness, gap widths, tube

thickness, coolant channel dimensions, eccentricity, fuel enrichment, fuel density, instrumentation errors (gag), etc. The results of this survey in terms of peak fuel temperature are given in Table 2.

It is a conclusion of this part of the review that from the purely thermal performance viewpoint the tubular elements are all greatly superior to the hollow rod and that the tubular interacting design is the best of the tubular designs.

THE MECHANICAL BEHAVIOUR OF FUEL PINS

The stresses arising in fuel element pins may be grouped conveniently under four headings:

- (a) Membrane stresses in the body of the fuel tubes.
- (b) Interaction stresses between the pin and its boundary wall due to bowing effects.
- (c) Interaction stresses, if any, between the fuel body and its boundaries (the fuel tube walls).
- (d) Discontinuity stresses at the ends of the pin.

All these stresses are dependent upon the temperature and fast flux gradients existing in the components, and the material properties. Of these latter the most important appear to be shrinkage, coefficient of thermal expansion and the elastic modulus. It is important, for a proper understanding of the behaviour of fuel pins and to avoid serious error that the method of analysis should take due account of the variation of these properties with irradiation dose and temperature.

Within the Project analytical methods have been developed for estimating the membrane stresses (a) and interaction stresses (c) as dose dependent functions, whereas existing methods for the interaction stresses (b) and the discontinuity stresses (d) are still under development. Project work so far has indicated that peak stresses due to (a) and (c) are reached in the dose range 4 to 8×10^{20} E.D.N. and we could expect discontinuity stresses to behave similarly. Interaction stresses due to the bending of the pin within the coolant

channel may however, be expected to reach their peak values at doses which will be dependent on initial clearances and this dose may or may not be in the same range. For some design proposals, for example fuel pins without external ribs, interaction with the channel walls may be entirely absent. From the calculations made so far in support of test elements for use in the Dragon core none of the stresses, (a), (c) or (d) occurring in the fuel tubes appear to give cause for concern; peak tensile stresses of about 700 psi, under shut down conditions and after operation for about 100 days or more, being typical for pin designs not very dissimilar to the power reactor concepts. On the other hand interaction due to bending (b) can apparently lead to relatively high stresses if the temperature gradient and/or fast flux gradients are sufficiently large, whilst the predicted stress (c) in the fuel matrix for one interacting design studied, suggests that "failure" of the matrix might be expected even in normal service. Accident conditions such as loss of coolant will result in increased interaction stresses of this type due to the flattening of temperature gradients between fuel and graphite sleeve.

With reference to the likely "failure" of the fuel matrix, it is the Project's present opinion that such failure will occur entirely within the matrix material and that it is unlikely to produce significant effects leading to fuel particle failure with enhanced fission product release. This latter point will of course be the subject of tests to be carried out by Dragon and must be positively proved before full advantage can be taken of it in pin design, (i.e., interacting tubular designs).

The predicted variation in typical membrane (a) and interaction stresses (c) as a function of dose are presented in Figs. 10 and 11.

Most of the effects giving rise to stresses in the fuel pins are common to all designs. The additional effects appearing in the interacting tubular design have already been discussed. The teledial design introduces one more additional effect due to the presence of the ligaments between the fuelled holes. In the ligament region the

graphite is hotter than either of the boundary walls, so that initial thermal effects will give rise to increased stresses in the walls, tensile at the outer wall and compressive in the ligament and inner wall. Subsequent irradiation induced shrinkage and a reversal of the thermal effects at shut down after creep relaxation effects have been taken into account, ultimately result in tensile forces in the ligament region with corresponding compressive stresses in the outer boundary wall and tensile stresses in the inner wall. An initial calculation of these effects suggests that the stresses arising will be within the acceptable limits.

Future reactor systems will be called upon to follow load demands as fully as possible. Such a requirement together with the small variations in power inherent in generating and supply networks impose cyclic stresses on the reactor components, particularly the fuel element pins of the reactor core. Wherever such cyclic effects may be imposed it is inevitable that the fatigue performance of the materials directly involved are questioned and it becomes necessary to relate fatigue performance to allowable stress levels.

Typically, stresses in fuel pins during steady state operation at full power are quite modest, about 5% to 15% of the ultimate strength of the material, whilst shut down (peak) stresses are normally about 50% of the ultimate strength and could rise in some designs to about 70% or so of the ultimate strength. Cyclic power variations can then be expected to produce stress changes equal to the product of the fractional power change and the difference between the peak (shut down) stress and the operating stress.

Various types of fatigue test on representative graphite specimens have been carried out by Sud-Aviation on behalf of Dragon Project. These tests took the form of bend tests with a stress variation imposed upon a non-zero mean stress.

The results are plotted in the Goodman type plot shown in Fig. 12, in which the ordinate is the ratio of stress amplitude or range to ultimate strength and the abscissae the ratio of mean stress to

ultimate strength.

On the same figure the region of operation for typical pin stresses has been indicated. Electricity supply utilities have indicated that a maximum of about 10^6 power cycles in the range 1% to 60% power (reduction) should be expected in the lifetime of a fuel pin in a power reactor. It may therefore be deduced from the data presented in Fig. 12 that the safe peak stress could conservatively be as high as 0.7 of the ultimate strength of the material.

Before leaving the subject of fuel pin stresses it is important to re-emphasise that significant changes in any of the material properties could materially alter the predicted values. For example a reduction in the creep coefficient of the materials will increase the operating stress levels pro rata and increase the dose required to reach maximum stress levels. In this context it should be noted that data on the material properties of the fuelled matrix are at this time somewhat tentative and much work remains to establish shrinkage, thermal expansion and elastic modulus data over the full range of temperature and dose. Particularly, creep data on matrix fuels is non-existent and in Dragon calculations to date it has been assumed that creep coefficients for the matrix are the same as those of representative graphites.

CONCLUDING REMARKS

Limited improvements in fuel element performance can be brought about by careful attention to the relative importance of the various material properties. For example in some design concepts it may be advantageous to have better thermal conductivity in exchange for a worsening irradiation shrinkage. It would seem, however, that the greatest benefit is to be achieved by choosing that fuel pin which combines the best possible thermal performance with the least number of operational unknowns requiring life tests in the core of a reactor. Of the designs discussed in this report the teledial and to a lesser extent the tubular non-interacting fuel pin concepts seem to fall nicely into this category, whilst to take full advantage of the better

thermal performance of the tubular interacting design it will be necessary to demonstrate in full life tests that the interaction effects between fuelled matrix and graphite sheath do not lead to an unacceptable deterioration in fuel performance.

The teledial design is only slightly less superior in thermal performance and will require far less in-pile testing to prove the integrity of the fuel containing body. The tubular non-interacting design has a similar thermal performance but its optimum design will rely upon sure knowledge of the relative shrinkage of its component parts, graphite and fuel body. This design may also suffer somewhat from the time dependent character of the heat flow into the inner and outer channels. The hollow rod concept suffers principally from its relatively poor thermal performance and like the tubular non-interacting design requires a relatively precise knowledge of the dimensional changes to be expected in the fuel body and graphite sheath if allowable peak random temperatures are not to be exceeded. Manufacturing costs, on the other hand, are expected to be least for the hollow rod and to increase for the tubular designs with the teledial as the most expensive. All these factors must be brought together in optimised core designs before a final picture of the relative merits of the various designs can be obtained.

Table 1. Comparison of the Designs in a 68 mm Channel
(Nominal Peak Fuel Temperature = 1225°C)

(Start of Life Conditions)

Design Type	Core Power Density MW/m ³	Core Pressure Drop g/cm ²	Peak Channel Power kW	Peak Surface Temperature °C
Hollow Rod	4.45	150	304	935
Tubular (N.I.)	7.80	302	460	965
Teledial (9-hole)	9.25	500	532	983
Tubular (I)	9.50	365	526	976

Table 2. Comparison of Peak Fuel Temperatures During Life

Design	Nominal $^{\circ}\text{C}$	Systematic $^{\circ}\text{C}$	Random $^{\circ}\text{C}$	Approx. Core Power Density MW/m^3
Hollow Rod	1320	1400	1545	5
Teledial (8-hole)	1145	1170	1250	7
Tubular non-interacting	1200	1225	1330	8
Tubular interacting	1170	1200	1295	9

1. Notional Design of Fuel Pins

2. The Hollow Fuel Rod

1. MATRIX FUEL COMPACT
2. INNER GRAPHITE SLEEVE
3. OUTER GRAPHITE SLEEVE
4. GRAPHITE END CAP

3. The Tubular (Interacting) Fuel Rod

1. MATRIX FUEL COMPACT
2. INNER GRAPHITE SLEEVE
3. GRAPHITE BODY
4. GRAPHITE END CAP

4. The Tubular (Non-interacting) Fuel Rod

1. MATRIX FUEL COMPACT
2. GRAPHITE BODY
3. GRAPHITE END CAP

SECTION A-A

SECTION B-B

5. The Tubular (Teledial) Fuel Rod

6. Hollow Rod Characteristics in 68 mm Channel

7. Tubular (Teledial) Characteristics in 68 mm Channel

8. Tubular (Non-interacting) Characteristics in 68 mm Channel

9. Tubular (Interacting) Characteristics in 68 mm Channel

10. Variation of Membrane Stresses with Irradiation Dose

12. Limiting Stress Amplitude as a Function of Mean Stress

11. Variation of Interaction Stresses with Irradiation Dose

DISCUSSION

J. L. Scott: Do you orifice the flow for the tubular interacting or teledial designs?

E. Smith: Normally, no. Using the calculation techniques available it is possible to obtain optimized geometries in which either the fuel temperature or the temperature difference between the inner and outer surfaces in tubular designs is minimized. We have noted that in optimizing for minimum temperature differences the penalty on maximum fuel temperature is usually quite small, say about 10°C.

H. Gütman: I should only like to answer the questions concerning our calculations of the age factors. They were deduced from the fission cross sections applying an average spectrum over the whole irradiation time. Irradiation-dependent disadvantage factors were taken into account. We have not yet performed supercell calculations. Due to this there is a certain uncertainty in our age factors, but comparisons with the age factors calculated by the consortia have not shown any inconsistency.

J. Scarborough: Have the Dragon fuel design assessments proceeded to the point of developing reasonable estimates of fabrication costs for each of the optional designs discussed, and if so, could you comment on the relative cost of each?

E. Smith: The question on relative costs will be answered more fully in a paper to be given by H. Gütman of the Dragon Project (paper 6/134, Session VI). However, I may say that relative costs have been assessed, but for the designs discussed in the paper relative production costs do not have a large influence on overall generating costs which are much more influenced by variations in allowable core power densities, carbon to uranium ratio, heavy metal density in the fueled space, etc.

J. D. Thorn: Please explain the basis for selection of linear scale for the various geometries, and comment on how far the comparisons drawn are arbitrarily affected by the choice of scale. Obviously if different sizes had been selected different answers would have been obtained.

E. Smith: In the preliminary analysis presented, the choice of 68 mm for the channel size was indeed somewhat arbitrary and somewhat different quantities would result from a different choice of scale. However, the analysis was carried further and each of the designs was examined with respect to thermal performance in a variety of scales in which channel diameter and pitch were the principal variable, with a fixed core height-to-diameter ratio. The results of that more complete analysis did not change the original conclusion that tubular designs were far superior to the hollow rod with the tubular interacting design as best, closely followed by the teledial. What the additional analysis did indicate was that the hollow rod would be better in smaller channels and that teledial would be more appropriately disposed in somewhat larger channels.

J. A. Robertson: In tubular designs the split in heat flow between inner and outer surfaces is sensitive to assumptions about heat transfer between fuel rods and graphite containers. Where the split depends on calculation is it necessary to allow for larger uncertainty factors that offset apparent benefits of this geometry?

E. Smith: We think not. While variations in theoretical assumptions appear to change the inner and outer graphite temperatures there is no evidence of large effects on peak fuel temperature. We are fairly confident of our theoretical prediction for the geometries but will be backing the calculations up with observations from experiments now in the Dragon Reactor and by experiments proposed for further charges of the core. It is, for example, our intention to measure in some experiments internal, external, and mixed gas exit temperatures, and to obtain coolant flow split data for additional out-of-pile tests on representative channels.

THE DEVELOPMENT AND PERFORMANCE OF HTR CORE MATERIALS

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ABSTRACT

The cores of prismatic High Temperature Gas Cooled Reactors are composed of structural graphite fuel blocks and tubes containing coated fuel particle assemblies. The design and performance of the coated particle is described in terms of the potential sources of fission product release together with the implications of the choice of coatings and of chemical and stressing effects occurring during service. The reasons for favouring the incorporation of the fuel particles into graphite matrix compacts are discussed and data presented on dimensional changes experienced during irradiation. For the structural graphite, the available commercial materials are adequate for the early launching of HTR power systems and the service conditions applying in these reactors offer scope for further improvements in the economy of power production.

INTRODUCTION

The HTR may be viewed as part of the logical development of gas-cooled reactor technology. The concept of the use of an inert coolant, helium, coupled with a core construction entirely of refractory non-metallic materials is not only attractive from the materials viewpoint but leads to a design which is inherently safe. Following initial work in the United Kingdom in the mid 1950's which resulted in a proposal for a reactor experiment incorporating these principles¹, the OECD High Temperature Reactor Project, Dragon, was set up in 1959. The tasks of the Project were to construct and operate a reactor experiment and to develop knowledge of the system to such a point that the Signatory Countries could assess, design, build and operate power producing HTR's to their own specific needs with the minimum of additional research and development.

During the eleven years which have passed since the formation of the Project, core material development has passed through several

stages, ranging from those specifically aimed at achieving successful operation of the Dragon Reactor itself through fuel and graphite development for thorium cycle systems, and more recently, work specifically geared to the early launching of power reactors utilising low-enriched uranium cycles. In this paper no attempt will be made to review each of these stages but the current state of knowledge relevant to power reactor performance will be emphasised. It can be stated, however, that materials work on the HTR system has passed from feasibility studies to the provision of data necessary for the design and construction of an economic power producing system.

Three factors may be considered as having been of paramount importance in the transition from the initial concept to engineering reality: the convincing demonstration of the use of helium as a primary circuit coolant, given by the successful operation of the Peach Bottom, AVR and Dragon Reactors; the development of fission-product-retaining coated particle fuel and the excellent behaviour of graphite fuel elements at high temperatures and high power densities. Helium technology and core design are discussed elsewhere in this Symposium and this paper will be confined to fuel and graphite.

COATED PARTICLE FUEL DEVELOPMENT AND PERFORMANCE

Concept

The function of the coated particle is to generate heat whilst retaining fission products to a degree that ensures the safe operation of a power reactor and permits maintenance of circuit components which are subject to fission product plate-out. The particles consist of spherical fissile or fertile kernels surrounded by coatings laid down by pyrolysis in fluidised beds. Kernels of both oxide and carbide composition have been developed and although most of the exploratory work by the Project was devoted to the development of uranium thorium dicarbide kernels, a decision was made in 1966 to concentrate on uranium dioxide for the exploitation of low-enriched cycle systems. Since fission gives rise to both a volume increase of the kernel material and to gas release, space must be made available within the

coated particle to accommodate these effects. Accordingly the Project favours building porosity into the kernel and current specifications for power reactors lay down a nominal value of 20%². The philosophy of the choice of coating materials has remained unchanged since the formulations made in the early 1960's and has been discussed by Huddle^{2, 3}. The functions of the several coatings working outwards from the kernel are briefly described below:

Pyrolytic carbon (PyC) buffer coating - to act as a catchment for fission fragment recoil from the kernel. Experience has shown that to avoid disruption of this layer (spearhead attack) specific properties of the coating are required and in the case of fuel of uranium dioxide composition it is required that the density of the PyC be approximately 1 g/cm³.

Inner PyC coating - the first of the three coatings which make-up the particle's pressure vessel. This again requires specific properties as shown later, to maximise the useful life of the particle in the environment of a fast neutron flux giving rise to dimensional changes and consequent stressing. In addition, this coating serves as a barrier to gross diffusion of kernel born material which may affect the integrity of the silicon carbide. With uranium dioxide fuel it is usual to place a PyC sealing layer between the porous buffer and high density inner PyC to avoid uranium carbide formation during subsequent coating operations.

Silicon carbide coating - the main diffusion barrier to metallic fission products. This coating also plays a significant role in the mechanical performance of the particle and in addition acts to give the particle dimensional stability through its stiffness and stability under neutron irradiation.

Outer PyC coating - the third member of the pressure vessel aiding in the mechanical performance of the particle but also providing chemical protection for the silicon carbide.

Fig. 1 illustrates the so-called Dragon Reference Particle (800 μm kernel 155 μm total coating thickness) which has acted as a focal point for the development of low-enriched power reactor studies. It should not be assumed, however, that this represents the optimised particle for this system; it merely acts as a reference and was defined at a particular point of core design development. In fact the geometry of a particle used in a power reactor will be a compromise between the ultimate in performance (small kernel, thick coating) and the engineering and physics parameters of core design which may tend to require maximisation of the investment of heavy metal in the fuel space (large kernel, thin coating). Power reactors of the low-enriched variety now being designed require uranium investments in the fuel space in the range 0.7-1.0 g/cm^3 leading to kernel sizes of approximately 650-800 μm and coating thicknesses from approximately 130-180 μm for the smaller kernel and 160-230 μm for the larger.

Fission Product Release Modes

In considering fission product release in HTR systems it should be recognised that a number of barriers or delaying features exist between the coated particle fuel and release into the environment i.e., diffusion through the fuel body, escape across the fuel body/graphite fuel sleeve gap, diffusion through and evaporation from the fuel sleeve and finally plate-out phenomena. Some of these features can be very powerful in attenuating fission product release from the reactor even in the presence of coated particle fuel failure but they are discussed elsewhere in this Symposium.

Concerning the coated particle itself fission product release may be broken down into three potential sources:

- (i) fuel contamination in the coatings
- (ii) diffusion of kernel-born products through the coatings
- (iii) escape direct from the kernel through broken coatings.

Release from Contamination

During manufacture a low level of fuel contamination occurs in the PyC coating. For the silicon carbide type fuel described, the only contamination of importance is that in the outer PyC layer. Typically this is at a level around 10^{-6} expressed as a weight fraction of fuel in the kernel. In the case of noble gas fission products many irradiation experiments show that releases of the order of 10^{-7} release rate/birth rate (R/B) are observed for those species with the longest half lives as a result of this contamination, due to the relatively low transport rates in pyrolytic carbon. The release of shorter lived species is corresponding less due to the delay in transport through the PyC. In the case of fuel particles embedded in graphite matrices, releases are approximately one order of magnitude greater, due, it is thought, to release from recoils into the surrounding matrix rather than from PyC. For metallic fission products the higher transport rates lead to releases which are approximately equal to the contamination level.

It can be stated, however, that these levels of releases are of no embarrassment to reactor operation and safeguards are provided by appropriate quality control tests in the fuel production.

Release by Diffusion through Coatings

Transport of fission products through the particle coatings is a complex phenomenon involving not only diffusion itself but also the partition coefficient of the fission products between kernel and PyC coating, between the PyC and SiC and finally the release into the surrounding matrix. The system has been treated theoretically for the Project by Walther⁴. Silicon carbide was chosen as the most suitable material to serve as the primary barrier to metallic fission products on the grounds that it was likely to possess far lower permeabilities to the metals than the flake-like graphite structure of pyrolytic carbon. Fig. 2 illustrates the powerful effect of a silicon carbide interlayer on the fractional release at equilibrium where the diffusion coefficient in the SiC is 10^4 lower than in PyC. To provide

data for model calculations, fission product transport in SiC is being intensively investigated by the Project in both laboratory measurements (by determining fission product profiles in irradiated coated particles) and through the post-irradiation examination of large assemblies of coated particle fuelled elements. In the latter case, analysis of the fission product data from all experiments so far performed by the Project has failed to detect a release component due to diffusion through the PyC/SiC/PyC type particle. In the case of particles coated only with PyC, however, strontium is found to escape readily at the higher operational temperatures as shown by the results given in Table 1 for two fuel elements, of a group of nine, irradiated in the Dragon Reactor for 280 full power days. (Burn-up approximately 3.5% FIMA). Companion experiments to these having double the irradiation time are being examined currently and it is planned to build-up even longer irradiation times in elements remaining in the Dragon core. In summary it may be said that fission products release occurring as a result of the diffusion of kernel-born species is not likely to be of major concern in currently-envisaged reactor designs where the residence time may be of the order of 800-1,300 days.

Release from Defective Coatings

From the previous sections it is apparent that the fission product release from intact PyC/SiC/PyC coated particles is very low and is not expected to control the situation in a power reactor. Breaching of the coatings, however, can be expected to lead to considerable increases in release. These are indeed observed in practice and, in the case of noble gases, are commonly used to monitor the fuel performance during irradiation experiments. It is not yet known in detail what degree of retention is afforded by the kernel when breaching of the coatings occur. Fig. 3, however illustrates the release of Xe isotopes observed when intact irradiated particles of the Dragon Reference type containing kernels of UO_2 with 20% porosity are crushed at room temperatures. In these experiments, performed for the Project at SGAE, Seibersdorf, Vienna, it was found that at relatively low burn-up (about 2% FIMA) the observed release was strongly dependent on temperature whereas at the

higher burn-up of 6.5% FIMA the release was high and not very temperature sensitive.

It is clearly of great importance, therefore, to define the performance limits of the coated particle fuel in terms of the presence of fuel with "failed" coatings since it is the fraction of such fuel in the reactor which will dictate the magnitude of the fission product release source. Ruptured coatings can of course be present in the fresh fuel as a result of manufacturing operations but this question will be dealt with later.

Coating failures during service may be divided into two separate areas although under some conditions of operation there is likely to be interaction between the two. The two areas are failures which are essentially chemical in origin and those which are mechanical.

Chemical Failure.--This can occur as a result of an interaction between the materials of the kernel and the coatings. For the two main fuel types, oxides and carbides, potential mechanisms are reactions between oxygen released during the fission process and the coatings for the former, and the mutual solubility or reactions between the carbide fuel and the coatings in the latter. In the case of UO_2 , direct reaction between the PyC and the fuel occurs at very high temperatures but would lead to excessive CO pressures leading to mechanical failure. The temperatures at which this occurs, however, are considerably above 2000°C and are not restrictive either in fuel manufacture or in the operation of reactors being currently designed.

A common feature of the chemical deterioration of coated particle fuels observed in very high temperature irradiations is the so-called "amoeba effect" in which the kernel moves through the coating in a unidirectional sense. This effect has been seen in early carbide fuels in Dragon irradiations and has also been reported by Goeddel⁵ for carbide fuels. The phenomenon is clearly associated with temperature gradients across the particles during irradiation and in the case of carbide fuel the direction of movement is up the temperature gradient. During the last three years a similar phenomenon has been observed in

Table 1. Comparison of Strontium Release in SiC and PyC Coated Fuel Particles

Fuel Type	Isotope	Fractional Release* at Fuel Temperature			
		900-1000 °C	1040-1150 °C	1160-1230 °C	1170-1240 °C
PyC	Sr-90	4.6×10^{-6}	1.7×10^{-4}	1.4×10^{-2}	9.1×10^{-3}
PyC/SiC/PyC	Sr-90	4.3×10^{-6}	3.0×10^{-6}	3.3×10^{-6}	4.7×10^{-6}

*Note Fractional release measurements made by analysis of graphite fuel sleeve surrounding fuel during irradiation.

Table 2. Results of Experiments on Amoeba Failure in UO₂ Coated Particle Fuel of Dragon Reference Design

Experiment	Particle Surface Temperature °C	Rating (W/Particle)	Estimated Cross Particle Gradient °C	Irradiation Times to Onset of Failure (days)
Risø LEHPD2 (Denmark)	1425	0.68	250	140
HTE (Dragon Reactor)	1650	0.26	125	85
HTE (Dragon Reactor)	1725	0.26	50	85
HTE (Dragon Reactor)	1900	0.26	125	8
Studsvik 16/1 (Sweden)	1650	1.4	280	10

UO₂ coated particle fuel in several experiments performed by the Project. Since in these experiments the fuel was irradiated in loose form it has not been possible to clearly distinguish the direction of the kernel movement but in one experiment there appeared to be a distinct **tendency** for the kernel to move down the temperature gradient. The occurrence of the amoeba effect in UO₂ coated particle fuel is being studied from the theoretical viewpoint but will not be pursued in this paper. Rather the results of some relevant experiments will be given showing how the failure appears to be empirically correlated with the parameters temperature, temperature gradient and particle rating. The experimental data results are presented in Table 2 and the correlation shown graphically in Fig. 4.

It should be emphasised that in all these experiments loose coated particles were irradiated, contained in annular cylinders machined in graphite. The heat transfer in such systems is complex⁶ and although amoeba effects should in principal be associated with the conditions within the kernel, in the above analysis the estimated temperature gradients refer to the coated particles. Further, the times refer to the onset of particle failure observed through an initial rise in the release of noble gases, and although in all the experiments the gas release increased progressively thereafter, only relatively few particles were found to have completely breached coatings at the end of the experiments. For example, in the case of LEHPD2 the irradiation was continued up to 240 days achieving 6.5% FIMA burn-up but only 1-2 percent of particles operating under the stated conditions were found to be failed at this stage. Other particles in this experiment operating at 1300°C and lower were unaffected. Further experiments are now underway in which the phenomenon is being investigated in particles embedded in a graphite matrix, this situation being more representative of power reactor conditions.

In using the correlation in Fig. 4, the designer would tend to use a design line displaced somewhat lower than that shown, to minimise the degree of amoeba penetration, so safeguarding the mechanical performance of the particles. Even so it can be shown that with appropriate fuel

element design, peak random conditions can be achieved in which amoeba attack is not the life-limiting feature.

Mechanical Failure.---In studying the stress history of a coated particle during operation many factors have to be considered. Stresses can first occur as a result of effects attendant upon burn-up in the kernel itself. The primary causes which might be expected are swelling and the generation of pressure due to fission gas release and, in the case of UO_2 , carbon monoxide. A second source of stress generation is in the pressure vessel coatings themselves as a consequence of fast neutron effects.

For the kernel, to avoid the bursting of particles arising from swelling, the Project favours the use of porous kernels. With the level of porosity at about 20% no deleterious effects due to swelling have yet been found in irradiation experiments even up to 6.5% heavy metal burn-up for recent UO_2 fuels and in excess of 10% for earlier fuels. To derive values for internal pressures generated by released fission gases, data of the type depicted in Fig. 3 may be used, with due allowance for volume changes within the particle. In the case of UO_2 fuel an additional component can arise due to the formation of carbon monoxide. Some recent measurements made in parallel to those for the fission gases show the presence of CO in irradiated particles, revealing both a dependence on the level of burn-up and on the temperature at which the particles were crushed. These results are shown in Fig. 5: they suggest that CO releases may become comparable to the fission gas levels at about 1500°C for particles with about 6% FIMA burn-up. It should be noted that the CO releases shown in Fig. 5 are the maximum values found. If the irradiated particles are maintained at temperature for longer periods prior to crushing, the measured release falls, reaching half the values shown after 2 hours at temperature. The reason for this is not understood: it may be related to the establishment of chemical equilibria involved in the amoeba phenomenon.

Stresses in the PyC/SiC/PyC coatings arise not only as a consequence of the build-up of internal pressure but also from

differential dimensional changes caused by fast neutron irradiation. In general, PyC undergoes considerable dimensional changes under these conditions⁷ whereas the pyrolytic SiC is relatively stable⁸. These effects lead to the development of circumferential tensile stresses in the PyC, which are balanced by irradiation creep, and relatively high compressive stresses in the SiC. This stress acts in opposition to the effects of the internal gas pressure and delays the point in life at which tensile stress is developed in the SiC. Since the dimensional changes in PyC are strongly dependent on the isotropy and initial density, specification of these features play an important part in the particle optimisation.

To study the stress history of coated particles during service and to assist in the design of experiments related to optimisation, a mathematical model has been developed for the Project by Walther⁹ (SORIN, Italy). The results of a typical calculation showing the general effects expected in a reference particle, are shown in Fig. 6. It should be noted that current low enriched HTR designs are associated with burn-ups in the range 6-9% FIMA and fast neutron doses between 3 and $4 \times 10^{21} \text{ n cm}^{-2}$ (DIDO Nickel Equivalent). Failure criteria identifying the end of the useful life of coated particles have not yet been fully verified experimentally. It is common practice, however, to regard the particle life for power reactors in terms of the point at which a state of zero stress exists in the silicon carbide, thus allowing for stress increases near the end of life associated with reactivity transients. To provide experimental verification of model calculations leading to detailed particle specifications for use in power reactors, the Project has in hand a large programme of irradiations in both the Dragon Reactor and in extra mural experimental facilities. This programme covers the major variables of kernel size, kernel porosity and coating thicknesses. It should be pointed out, however, that a wide range of early varieties of coated particle fuels have already completed irradiation periods in excess of 500 days in the Dragon Reactor without displaying progressive failure in the fission gas monitoring system. In these experiments temperatures from 700-1200°C

have been covered with peak neutron doses of $3 \times 10^{21} \text{ n cm}^{-2}$ DIDO Nickel Equivalent and burn-ups in excess of 10% FIMA being achieved at the higher temperatures. These results give confidence in the ability of coated particle fuels to meet the performance requirements of reasonably pitched designs.

THE FUEL BODY

Two methods of locating the coated fuel particles within the fuel element structure have been investigated by the Project. One technique termed "meniscus bonding" involves precoating the particles with a thin coating of graphite powder/resin mixture, pouring the particles into the required fuel space, and heat-treating to carbonise the resin binder. This method may be utilised where it is required to achieve maximum heavy metal investment in the fuel space. It was specifically developed for HTR core designs in which the moderator stack was fixed and contained conventional fuel channels (the so-called heterogeneous type of design). Current designs, however, are of the homogeneous type in which the graphite fuel containers are located in channels placed symmetrically in graphite blocks which are removed together with the fuel at the end of life. In these designs, the volume of the core available for the fuel is much greater than in those of the heterogeneous type and the specific heavy metal investment requirement within the fuel space is consequently much lower. This relaxation can be used to advantage with the alternative fuel body, the graphite matrix type, in which the coated particles are embedded in a graphite matrix of relatively high density. Such a fuel body was in fact developed for the Dragon Reactor itself and has been found to perform excellently¹⁰. It also possesses features which overcome some of the disadvantages of the meniscus bonding: heat transfer through the fuel bed is improved thus aiding in the minimisation of fuel temperatures and temperature gradients; the unit is relatively robust and particle loss into the primary circuit is less likely in the event of fracture of the fuel sleeve; the graphite matrix acts as a barrier minimising mechanical and chemical interactions between adjacent particles;

finally the graphite matrix may serve to attenuate the release of some fission products even in the event of fuel failure. A potential disadvantage of the matrix fuel body is that to attain a reasonably high matrix density giving good mechanical and thermal properties, it is necessary to form the shape by pressing, involving some risk of particle breakage. To minimise this risk the Project has developed the so-called overcoating route in which the particles are coated with the graphite/resin mix to the correct degree, giving a cushioning effect during the forming operation. With proper attention to the graphite powder type and resin composition, forming pressures can be kept low and particle fracture below the presently required fractional limit of 10^{-4} .

The performance requirements of the fuel body differ according to the design detail of the fuel element as discussed by Smith¹¹, and it is not within the scope of this paper to discuss all aspects of the properties which have been covered elsewhere¹⁰. Perhaps the most important requirement is a knowledge of the irradiation induced dimensional changes since these can have a strong influence on the heat transfer behaviour through gap development and in mechanical interactions with the graphite fuel tubes. In this context it is the diametrical dimensional changes which are of most importance. To indicate the magnitude of these Fig. 7 is presented which shows the dimensional changes observed in overcoated graphite matrix fuel annuli irradiated in the Dragon Reactor. Although these results refer to compacts containing PyC/SiC/PyC fuel particles at 20% volume loading, other results suggest little influence of volume loadings up to 34%. However, it has been found that in the case of compacts containing fuel particles with plain PyC coatings, the irradiation induced shrinkage can be about 30% greater.

GRAPHITE

The main graphite components in homogeneous HTR's under current consideration are fuel blocks of about 400 mm width and one metre long containing axially bored fuel channels locating fuel tubes or sleeves containing the fuel compacts. In spite of the fact that power densities

in such systems can be up to 4 or 5 times greater than heterogeneous designs of gas-cooled reactors, with consequent reductions in core size, the peak fast neutron doses which will be experienced ($3-4 \times 10^{21} \text{ n cm}^{-2}$ DIDO Nickel Equivalent) are only one quarter to one fifth of those faced in the fixed moderator designs. This is brought about by not only because the fuel blocks are discharged together with the fuel but also because the fuel is spread over a greater volume of the core. The special features of the HTR relative to previous gas-cooled reactors are that the graphite structure operates at higher temperatures and the fuel sleeves are required to carry a substantial heat flux.

The Project's work on graphite has ranged over a wide field covering the development and performance of fuel tubes of controlled permeability for the Dragon Reactor^{12, 13, 14} to a studies of a very wide range of materials for potential use in power reactors. In this latter, it has been necessary to cater for a number of different power reactor design possibilities and to keep in mind the time-scale of design data accumulation and commercial availability. In anticipation of commercial exploitation, a decision was made in 1966 to concentrate on one type of graphite to allow time to develop the necessary design information. The main vehicles for the irradiation studies throughout have been in the HFR at Petten in Holland and in the Dragon Reactor. Lead experiments had shown very encouraging results in the temperature range $600-1200^{\circ}\text{C}$ for a graphite based on gilsonite coke, particularly in terms of the isotropic nature of the dimensional changes under irradiation. Also, this graphite has a high strength and is therefore attractive as a structural material in the core. A considerable body of information has now been gathered on the performance under irradiation of gilsonite-based materials and it is likely that this type will be chosen in the first prismatic power HTR's. Reviews of relevant data on this type of material have been given by Everett and Blackstone et al.,^{15, 16, 17} covering the performance under irradiation of materials produced by several European graphite manufacturers. In addition, a programme of work investigating the fatigue behaviour of the graphite has been initiated in view of the potential for HTR

systems to be employed under conditions of power cycling. Some preliminary results of these experiments which have been performed for the Project by Sud Aviation, Paris, are shown in Fig. 8.

The thermal and mechanical performance of several types of fuel element designs in which gilsonite based graphite is used, is discussed by Smith¹¹. Such analyses show that this type of material can be expected to give satisfactory and adequate performance in HTR power systems. In the future, however, two areas of improvement are envisaged. Firstly, since the peak neutron doses in the fuel carrier blocks in projected power reactors is relatively low, it is not unlikely that some sacrifice could be made in the dimensional stability if this was related to the provision of a cheaper material. Worthwhile reductions in the fuel fabrication costs could then be made as the graphite is a significant cost item. The second improvement relates to the fuel sleeves which have to carry substantial heat fluxes. Analyses show that for gilsonite graphite the reversed thermal stress occurring in the sleeves on shutdown is a more significant component in the total stress than that induced by irradiation effects. This arises because the dimensional changes induced by irradiation are small and the irradiation creep process is efficient in relaxing stresses caused by differential dimensional changes. On the other hand the coefficient of thermal expansion of gilsonite graphite is high, leading to a relatively high level of thermal stress. Although this is rapidly relieved during operation, it returns on shutdown in a reversed sense, adding to the residual irradiation stress. This situation could be improved by selecting a graphite for the fuel sleeve possessing improved thermal stress properties, even if this is at the expense of some loss in the dimensional stability under irradiation. Current graphite irradiation programmes have been cast with these improvements in mind.

CONCLUSIONS

Fuel and structural graphite materials development work for the HTR has passed the stages of innovation and feasibility and most of

A THE SUPERFICIAL APPEARANCE

B A POLISHED SECTION

C THE FRACTURE SECTION OF A COATING BY "STEREOSCAN"

1. Illustration of the Dragon Reference Coated Particle.

the current experimental work is associated with the provision of detailed design data for the commercial exploitation of the system.

For the fuel, the only significant source of fission product release is the presence of particles with failed coatings and thus most effort is concentrated on the definition of the performance limits in terms of both mechanical and chemical phenomena. In considering these effects, the designer should not ignore the importance of operational reliability to the utility and the incentive to achieve this by minimising fuel temperatures by appropriate design.

Commercial graphites are available on which design data exists and although certain improvements could be made, these materials are adequate for the construction and operation of power reactors.

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2. Calculated Fractional Release of Kernel-born Fission Products by Diffusion through PyC and PyC/SiC/PyC Coatings.

3. Release of Xenon Isotopes Observed on Crushing Intact Irradiated Dragon Reference Type Coated Particles at High Temperatures.

4. Onset of Particle Failure Observed during High Temperature Irradiation Experiments.

5. Release of Carbon Monoxide Observed on Crushing Intact Irradiated Dragon Reference Type Coated Particles at High Temperatures.

Dimensional Changes Observed in Graphite Matrix Fuel Compacts Irradiated in the Dragon Reactor.

6. Calculated Stresses Occurring in the Coatings of PyC/SiC/PyC Coated Particle Irradiation at 1200°C. Particle Parameters: Kernel UO_2 , 780 μm diameter, 18% porosity: coatings, buffer PyC 30 μm 1.1 g/cm^3 , inner PyC 56 μm 1.8 g/cm^3 , SiC 38 μm , outer PyC 34 μm 1.8 g/cm^3

8. Fatigue Failure Data for Gilsonite Graphite Subjected to Cyclic Stress at Room Temperature.

DISCUSSION

S. I. Kaplan: What was the kernel composition for the correlated data on amoeba-effect and failure vs $1000/T$? Have you correlated data for any other kernel composition?

L. W. Graham: The kernel composition was uranium dioxide. In previous irradiation experiments at temperatures above about 1600°C , we have seen this effect with Th/U oxide fuel and in fuels with carbide composition, but there are not enough points to set up a correlation.

H. G. MacPherson: Could you say something about what you found on graphite fatigue?

L. W. Graham: There was not time to include this data in the presentation but you will find it in the paper. The shape of the fatigue curve suggests that graphite behaves intermediately between a brittle cast iron and mild steel. The implication of the data on cyclic stresses produced by power cycling in HTR fuel pins is not serious and is described in detail in E. Smith's paper.

M. T. Morgan: What causes the disappearance of the CO during the measurement when coated particles are crushed at room temperature, and doesn't the reaction between UO_2 and carbon create more CO during measurements at high temperatures?

L. W. Graham: The absence of CO on crushing at room temperature was attributed to absorption effects in the kernel and pyrocarbon. For the second part of your question, the particles are crushed in a mass spectrometer and the measurement completed in 1 to 2 seconds after crushing. Also the particles are quenched rapidly just after crushing. We have not seen additional CO from the reaction of UO_2 with the PyC for crushing temperatures up to about 2000°C , the maximum we have looked at.

C. B. Zitek: Had any fissile material been found on the metal samples?

L. W. Graham: A very small amount was present; however, much more was found in or on the graphite samples - less than 10 grams in all the graphite in the core.

CHOICE OF FUEL DESIGN FOR HOMOGENEOUS LOW ENRICHED HTR

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6137/00

ABSTRACT

The paper considers firstly the specification of particle for a commercial HTR design and concludes that an 800 μ m particle will be satisfactory and possess sufficient endurance to meet the economically desirable irradiations.

These particles can be incorporated into compacts having sufficient strength, heavy metal density and low breakage fractions to meet engineering and radiological requirements. The irradiation shrinkage behaviour is reasonably matched to that of fuel tube graphite.

The incorporation of these compacts into various fuel element geometrics is discussed and it is concluded that all the various configurations available meet limits which are either set by excessive fuel temperature, graphite stress or compact stress. Bearing these limits in mind, it is concluded that, economically and technically, the tubular interacting design of fuel pin is the preferred choice. As an alternative, the integral block design also appears to be economically attractive.

INTRODUCTION

Construction of the first large high temperature reactor in the UK will be undertaken in 1971 and decisions are now being made on the basic parameters, so that the manufacture and proving of fuel elements and certain of the engineering features may begin. The reactor will be of 640 MW output approx and will incorporate prismatic fuel elements of a design agreed in its essential features throughout the industry.

Several alternative designs have been studied in considerable detail over the past two years and this paper is a condensation of the work done by TNPG and its associated companies.

Fundamental to the fuel element is the requirement for high rating associated with high burnup and low circuit activity without the fuel fabrication cost becoming too great and therefore uneconomic. It is considered that the right balance between these demands can now be made.

COATED PARTICLE DESIGN

The design of a reference coated particle was described in Ref 1 and the main parameters of a slightly revised reference design are set out in Table 1. This coated particle has a nominal $800\mu\text{m}$ kernel and coating thickness of $160\text{--}170\mu\text{m}$. There are economic incentives which lead to the choice of large kernel sizes. These incentives stem from factors in fluidized bed technology, fabrication costs of the coated particle decreasing as kernel size increases up to a kernel diameter around $900\mu\text{m}$.

For higher kernel sizes the fabrication cost does not decrease much more. Moreover, the coating operation presents some technical problems for larger sizes and it is, for the moment, safe to consider $800\mu\text{m}$ as a manufacturing limit for the kernel diameter.

The particle size influences the heavy metal loading which has an impact upon capital and generation costs, higher loadings leading to reduced costs. This also pushes one towards large particle sizes.

Accordingly, the 800 μ m kernel has been adopted as a standard and the main concern is to specify the coating thicknesses.

The main purpose of the coated particle layers is to retain the fission products throughout the design life of the element. The detailed function of the individual layer is well known and has been described before. Their thickness and important parameters are to a large extent determined by what is feasible in manufacturing and the design set out in Table 1 does not necessarily represent an optimum from the point of view of endurance.

This particle is a design which has been developed by Belgo-Nucleaire and is very similar to the one which is being incorporated into the TNPG element designs. The mechanical endurance of the particle is a complex function which, however, is amenable to calculation using computer techniques and this section of the paper briefly reviews the results of one such survey.

COCONUT (COating COmputation for NUclear Technology) is a computer code which calculates the stresses and strains on the coating layers and has been developed by BelgoNucleaire since 1967.

It proceeds through successive time steps and has been written for any fuel, coating material, numbers of layers, temperature and irradiation evolution.

The stresses and strains in the layers are generated by the fission and CO gas pressure, fuel swelling and the dimensional changes induced by the thermal history and the irradiation damage.

At each time step the stresses and strains are corrected for creep effects induced by irradiation and temperature.

For high power density conditions, the stresses induced by the thermal flux through the coating are also considered.

Using this model it can be shown that failure of the PyC/SiC/PyC particles occurs where the SiC layer goes into tension. Up to that moment it is kept in compression by the combined shrinkages of the inside and outside HD PyC layers.

This sudden increase of the hoop stresses in the SiC is due to the hydrostatic pressure induced in the coating by the kernel swelling.

Fig 1 Particle Stress History, shows the variation in hoop and radial stresses of the PyC and SiC layers which characterises this effect. It can be seen that the increase in SiC stress is very sharp and, although SiC has a relatively high tensile strength the point of failure can, to all intents, be taken as the time it starts to go into tension.

The effect of the kernel swelling, and therefore the failure of the particle, can be delayed by increasing the buffer layer thickness; Fig 2 gives the buffer thickness required to avoid this hydrostatic pressure before 4×10^{21} EDN; this buffer thickness is a function of the percentage of theoretical density, of the maximum fuel burnup, of the temperature and of the buffer layer density (the curves are shown dotted below the minimum thickness required to attenuate the fission products recoil).

The life of the particle can also be prolonged by changing the inner and outer HD PyC and by small changes to the HD PyC density but the effect of these parameters is less pronounced.

The characteristics of the particle in Table 1 have been developed for BelgoNucleaire fabrication process and are different from the characteristics of an optimum particle which might be given by the parametric study for two reasons:

- (a) The fabrication process of the reference particle is finalised; the fabrication process of an optimum particle is still under development.
- (b) The influence of the variation of most of the parameters is insignificant and a small change of N_g/N_u could lead to the same effect on particle lifetime, by altering fast neutron dose.

The development aspects of particle fuel are discussed in a companion paper² to this conference.

Thus it can be seen that the result of applying these calculations to the reference particle is to obtain theoretical burnups of the particle of the order of 10% FIMA which corresponds to about 60 to 70,000 MWd/teU mean reactor discharge irradiation. In fact it can be shown on most normal types of fuel element that the theoretical optimum burnup of a fuel element is around 60,000 MWd/teU as indicated by Fig 3. This corresponds to 3x rather than 4×10^{21} EDN. It can therefore be taken that the coated particle has sufficient endurance to meet the demanded irradiation which is also that which is economically desirable. The difference between predicted FIMA and fast neutron dose to failure and the levels which will need to be reached in practice is thus about 25%. This can be regarded as a design margin and should also take account of sub-standard particles. The endurance predictions are most firm for elements running at temperatures of approximately 1,200°C where there is sufficient experimental evidence to confirm the data fed into this computation. Extrapolation to higher temperatures is possible but increasingly uncertain and at the present time it is concluded that peak random temperatures of 1,400°C are as far as one would wish to extrapolate. On the other hand, there is satisfactory irradiation evidence for particles at this temperature so that reliance solely on a computational model is not necessary. This state of affairs is relevant to the choice of fuel element which is discussed later in this paper.

Thus, to summarise, it is evident that 800 μ m kernel coated particles have predicted endurance of:

Peak particle burnup	approximately 10% FIMA
Peak particle fast neutron dose	approximately 4×10^{21} EDN
At peak particle temperature	approximately 1200°C

Much irradiation evidence is already available up to these levels and more will arise from Dragon, STUDSVIK and OSIRIS by the end of 1970. UK programmes will provide still further data up to maxima of 8-10% FIMA/ 6×10^{21} EDN by mid-1971 and there will thus be ample corroborating

evidence for a reference particle by the fuel freeze date.

The significantly lower levels of endurance which are economically required for the first-off reactor should lead to a very conservative design in which there is a high degree of confidence.

DESIGN OF FUEL COMPACT

Ref 1 described a design of fuel body in which the particles were bonded together by a thin carbonised bond. Further development of this type of bond has been temporarily suspended since the method had low thermal conductivities and hence gave high fuel particle temperatures. It was also difficult to ensure the bond had sufficient integrity to avoid disintegration of the bed under irradiation conditions, whilst at the same time not being so strong as to adversely affect the particles. Finally, the change to a more homogeneous type of reactor design reduced the requirement for very high heavy metal loadings, of the order of 1.5 g/cc. The present proposals are to utilise the well proven compact-type of fuel body which has both high thermal conductivity and high mechanical integrity.

The design of the fuel element affects the specification for the compact body in a number of important ways as discussed below.

Shrinkage Data

One of the important parameters of the fuel compact is the shrinkage which it experiences under irradiation. Ideally this shrinkage should be matched to that of the graphite. In this manner it is then possible to prevent the opening of the interface gaps, or in the case of tubular interacting elements lower the resultant compact stress levels. In practice, this matching is difficult to achieve. In the first place the graphite and compact temperatures are different and secondly manufacturing availability restricts the selection of material and the shrinkage data which therefore results.

The UK programme has been based on the use of isotropic Gilsocarbon as the fuel pin graphite with a needle coke matrix for the compacts.

Gilsocarbon was chosen early on in the programme on the basis that considerable irradiation shrinkage data was available to underwrite design calculations and also that it had a very low shrinkage which was considered to be a desirable feature of fuel tube graphite.

The compact, developed at Dragon, on the basis of Shell H 100 coke, underwent some changes both in material (of better availability) and manufacturing methods to make it more suitable for the production-scale processes of the UKAEA Springfields works.

The current UK situation is that the two materials are not exactly matched and, at the temperatures of interest for peak rated fuel, the compact shrinks more rapidly than the graphite to the extent of about 1% maximum differential by about 1×10^{21} EDN, the difference thereafter decreasing.

In an ideal world this would not occur, but with graphite materials such a result is perhaps as best as could be realistically expected, certainly for a first-off commercial system. There is obviously room for improvement but, as will be shown, this difference does not prevent the derivation of practical reactor fuel designs.

Even if it were possible to match these two parameters exactly, there would always be a residual uncertainty due to scatter in the as-manufactured data which might be as much as $\pm 20\%$. Therefore, in designing fuel elements the differences between compact and graphite behaviour must always be taken account of, together with the scatter which is inherent in production quantities.

Strength

Thermal stresses which are experienced by the compact in some of these designs range up to about 800 psi. These stresses can be developed from the internal temperature drops within a compact, which in the case of rod-type elements can be very considerable, or, in the case of tubular interacting elements, from the interaction strains with the graphite. In these cases, therefore, a reasonable degree of strength is needed. It is possible to attain UTS values of approximately 2000 psi

by going to matrix densities of the order of 1.7 g/cc. This is not a particularly difficult target from the point of view of manufacturing, although it does require relatively high compaction pressures. If very high heavy metal loadings are simultaneously required, this can result in large breakage fractions during manufacture.

Designs such as the teledial or integral block designs do not have such a requirement and matrix densities could conceivably be reduced in those cases.

Heavy Metal Loading

As pointed out above, heavy metal densities of 1.5 g/cc are not needed for economic homogeneous designs nor, in fact, could they be achieved with the compact fuel approach. Nevertheless, there is an incentive to keep the heavy metal density as high as possible since basically a lower density increases the N_c/N_u ratio away from the economic optimum desired.

The particle design chosen currently fixes a practicable upper limit of 1.0 g/cc for which a volumetric packing fraction of up to 40% is needed. With the matrix densities specified in 'Strength' above, this can result in measurable (more than, say, 1 in 10^4 ; it is difficult to measure levels lower than this) broken fractions of particles during compaction. If it is wished to improve this situation in order to keep the reactor primary circuit 'clean' then some reduction in packing fraction is required. Alternatively, it would be feasible to thicken the particle coating to make it stronger. Either route reduces the heavy metal density and increases costs. The maximum reduction in density is only likely to be to about 0.8 g/cc, but the extent of the cost increase is a critical balance between:

- (a) Uranium costs (both initial and replacement) which are determined by reactor core calculations.
- (b) Fuel fabrication costs, particularly replacement, which are determined by a variety of overall system parameters and are obviously specific to a particular design.

However, for the general range of element designs investigated, the cost penalty for reducing from 1.0 to 0.8 g/cc is not prohibitive and mainly arises from fuel cost variations. Table 2 shows the manner in which costs vary for a particular pair of tubular designs. Fig 4 shows the manner in which fuel costs vary for a constant heavy metal density but varying Nc/Nu ratio. (Note: Cost datums are different from Table 2 for clarity).

Thus, it can be concluded that even the current state of development will give compacts which have the following characteristics:

Peak shrinkage relative to fuel pin graphite	- 1% $\pm$ 0.2%
Compact matrix strength UTS (unirradiated) approx	2000 psi
Heavy metal loading	0.8 g/cm ³
Particle breakage in manufacture	approx 1 in 10 ⁴

These parameters are satisfactory for an economic design now. The intervening time until 1973 will be used to underwrite the existing experimental data and (hopefully) by refinements to improve the specification.

Irradiation data for annular compacts is currently available up to 3×10^{21} EDN and up to 1200°C. This is satisfactory for peak rated positions.

FUEL ELEMENT DESIGNS

The discussion in this paper is limited to prismatic fuels. Discussion of the Pebble bed type element is not included.

In all cases the elements are embodied in hexagonal graphite blocks and the question to be answered is the preferred geometrical form of the elements in those blocks.

The general form of all the designs below is indicated in Fig 5 and some of the main parameters of typical design points are set out in Table 3. In the case of the GGA design the fuel ratings have been chosen with a view to making the Nc/Nu ratio 240, this being close to optimum. Broadly three categories of embodiment can be discerned:

Fuel Rods Cooled on the Outside

This sort of element is exemplified by the Peach Bottom design, whilst in Great Britain the hollow rod design has been evolved. Due to fuel temperature limitations incurred by having thick compacts and high heat fluxes, the hollow rod concept can be developed towards clusters of smaller rods along the lines of the trefoil design as put forward by BelgoNucleaire. The trefoil is only a particular case of cluster designs which all follow the welltried route of increasing surface to volume ratio. There are typical examples of this approach in current UK and US reactor technology.

Tubular Designs

Improvements to rating and decreases in fuel temperature can obviously be achieved by cooling the compact on both sides and one therefore progresses logically to a fuel pin which is tubular in concept. Several varieties of this type are currently being canvassed. In its simplest form the compact fits loosely between the inner and outer sleeve and does not interact with either. Rating improvements may be obtained by allowing some degree of interaction with the inner sleeve and there is therefore an interacting version of this concept.

A step change can be made by combining the two tubular sheaths into a solid graphite annulus and embodying the fuel compacts into this solid annulus as discrete lumps. Such a design has been put forward by Dragon Project and designated the teledial design.

Integral Block Designs

All the above variants incorporate the fuel pins within channels in the fuel block but by inspection of the last variant i.e. teledial design, it can be seen that it is but a short further step to incorporate the compacts directly into the block with coolant channels interspersed. This approach is exemplified by the fuel element being developed by GGA for the Fort St Vrain Reactor. Whilst this cycle is a uranium-thorium cycle it is interesting to speculate upon the application of the design to a low enriched uranium cycle.

Thermal Considerations

The Coated Particle Design section points out that the design endurance of the coated particles is temperature dependent with the highest degree of confidence at peak particle temperatures in the order of 1200°C. However, there are considerable experimental results available at higher temperatures and a peak fuel particle temperature of 1400°C may be chosen as the upper limit. The main design problem with the fuel element designs considered therefore reduces to attaining the desirable fuel temperatures at the ratings which will give the most economic benefits.

With the high thermal conductivities of the fuel compacts the temperature drops through them are generally small. The gas side surface temperature drop is a function of heat transfer coefficient which is set from economic consideration. The major uncertainty, therefore, in fuel temperature is the temperature drop which occurs between the outer surface of the compact and the inner surface of the graphite tube. Simple mathematics ignoring radiation terms show that this temperature drop can be expressed in the form:

$$\Delta t = \frac{q}{k} \cdot [G_o + R (S_2 - S_1)]$$

where q = heat flux across the gap

k = thermal conductivity of helium in the gap

G_o = initial hot gap dimension

R = radius of outside of gap

S_1 = shrinkage of graphite

S_2 = shrinkage of compact

Typical shrinkage curves for graphite and compact give maximum differential between S_2 and S_1 of about 1%, occurring at 10^{21} EDN. With the fuel compact dimensions of the hollow rod shown in Table 3, this difference in shrinkage rate increases the gap by 200 μ m approximately. For typical peak heat fluxes of (say) 55 watts/cm² the resultant increase in temperature across the gap is approximately 170°C from start of life. Similarly, if one allows for the $\pm 20\%$ variations in

these two parameters there is an uncertainty of about 50°C on this figure. Such high values of temperature difference constrict the rating of the hollow rod and, even going to maximum allowable temperatures of 1400°C , the capital and generation costs of this design are the highest of those illustrated. Nevertheless, the simplicity of the design has a strong attraction and there is considerable irradiation experience to back up the selection of a design of this sort. For a first-off station, therefore, this design could be an attractive solution since it gives considerable advantages over current gas-cooled reactor technology and offers scope for considerable further development.

The broad lines of this development are indicated by the other variants of fuel elements put forward. All attempt to minimise the uncertainty in fuel compact temperature by one of the following routes:

- (a) By reducing the magnitude of the problem by reducing either R (to reduce gap widths) or q . Note that for constant volumetric fuel rating q is inversely proportional to R also.
- (b) By avoiding it altogether and allowing the compact to remain in direct contact with the graphite sheath.

Method (a) gives the greater scope for design ingenuity. Method (b) is represented by the tubular interacting design. Further development of this can be foreseen in which the compact bed is cast integrally with the fuel pin, but more manufacturing development of this route is required.

As an alternative, the graphite and compact data could be tailored for equal shrinkage. This method, although theoretically attractive, has not to date been actively pursued since it is constrained by manufacturing availability of the required materials; furthermore, there will always be residual uncertainty owing to scatter in the data.

However, if it is pursued it would be most fruitful to modify the graphite for slightly greater shrinkage. This should also have the advantage that the coefficient of thermal expansion will be reduced with consequent benefit to thermal stress problems (see next section).

Stressing Considerations

Before going on to consider the performance of various types of element it is worthwhile briefly drawing attention to the problem of thermal stresses in all these designs. The very high heat fluxes involved produce temperature drops throughout the graphite and result in thermal strains which at the high operating temperatures relax out, so that during reactor operation the graphite stresses are generally very low. However, at shutdown one gets a reversed tensile stress pattern through the thickness of the fuel tube which is proportional to coefficient of thermal expansion and which can be of considerable magnitude. This effect is illustrated in Fig 6, which shows typical operating and shutdown stress patterns for a hollow rod design.

It might be claimed that these thermal stresses, being secondary in nature, are not harmful and also that the graphite, not being notch sensitive, would not suffer severe cracking anyway. However, there is no doubt that the predicted stresses can be very high (certainly in the order of $0.5 \times \text{UTS}$ for the designs of interest) and for such a vital component as a fuel element it is judged prudent at this time to take account of them.

The graphite also suffers from shrinkage stresses due to the differential temperatures and fast neutron doses which occur throughout the walls of the fuel element. These produce similar but generally smaller stress patterns.

Bowing stresses can theoretically cause more serious effects since the restraint can stress one side of the tube in a completely tensile fashion¹. However, these stresses are generally low compared with the pure thermal stresses and, moreover, can be minimised by design e.g. increasing the rib/channel clearance. Moreover, there is probably a degree of self-stabilising anyway, in that having bowed towards a wall hot-spots will be developed on the fuel pin opposite the bow which will tend to straighten it out.

It is therefore concluded that thermal stressing of the graphite elements is the main mechanical problem. For comparison purposes

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simple 2-dimensional analyses at the mid-plane of the fuel pins are satisfactory but the more complicated end effects, where steep axial temperature gradients and varying restraint conditions occur, need special treatment.

DISCUSSION OF ELEMENT PERFORMANCE

As mentioned in Thermal Considerations above, methods (a) and (b) allow higher ratings with lower temperatures and all the designs considered here illustrate various ways in which this is obtained. Dealing with these designs in turn we may make the following comments:

Trefoil Design

This design reduces both R and q , thereby allowing considerably higher fuel ratings for roughly the same limiting fuel temperature. Obviously this improvement could be continued by increasing the number of pins still further. However, this course of action is likely to give reducing returns since the fuel fabrication costs will rise steeply with the increasing number of graphite components which have to be machined. There are likely to be mounting problems for large numbers of small pins within small channels and also the balancing of flows between the various sub-channels.

The impact of fuel fabrication costs can be seen (Table 3) from the generation costs which are high compared with other equally rated designs. Thus the trefoil may be regarded as indicative of this sort of trend.

Tubular Non-Interacting Design

This design halves q although, in the case shown, the value of R goes up marginally. However, on balance there is a nett reduction in gap temperature drop which allows reasonably high ratings with fuel temperatures in the neighbourhood of 1200°C . The main difficulty with this design is that, if it is regarded as being essential to avoid graphite/compact interaction, large initial gap allowances must be made to ensure this condition throughout life and for the expected

variability of material parameters. One is then trying to balance the effects of two gaps which are (thermally) in parallel. This can lead to wide variations in heat split and temperatures between the two coolant surfaces. These temperature differentials, apart from being inherently undesirable, can cause severe stressing problems in the walls and end caps.

Thus, although in principle the element design can produce attractively high ratings, in practice the engineering uncertainties involved in maintaining the non-interacting concept are likely to erode these advantages and there will always be a substantial degree of uncertainty in thermal performance predictions.

Teledial Design

This takes the trefoil concept further and embodies the compact into a single thick annular graphite holder. In doing so it reduces both R and q and theoretically very high ratings are possible with this design. However, since the design is essentially one of a heavily redundant structure the graphite stressing problems at the mid-plane of the element are quite severe. Fig 7 shows some finite element stress work which has been carried out by Agip Nucleare on a typical teledial design. This indicates that very high tensile stresses will occur on shutdown in the outer rim of the element and for this reason we conclude that the rating of the element needs to be limited.

Accordingly, the design point set out in Table 3 has been derated to an extent which meets a reasonable graphite stressing criterion. Being of a tubular design, there is again the problem of predicting inner and outer surface temperatures and imbalance in these can also lead to increased stress levels. Finally the larger amount of graphite present tends to produce marginally worse values of N_c/N_u .

Another aspect of the element is that the large numbers of very small compacts require slightly higher fuel fabrication costs to the extent of perhaps 10% compared with a hollow rod element.

All these factors militate against obtaining very low capital and generation costs, although the values set out in Table 3 are still quite attractive.

Integral Block Designs

For the purpose of comparison we have taken the GGA block as published and assumed that compacts can be fitted into this with a heavy metal density adjusted such that the overall Ng/Nu ratio is about 240 i.e. close to optimum.

A point of interest is that the utilisation of space within the block is very efficient so that the heavy metal density in the compact can in fact be reduced to values of the order of 0.6 to 0.7 g/cc and still attain the desired uranium to graphite ratios. Stemming from this very high space utilisation it is possible to reduce fuel volumetric ratings so that there are significant decreases in both R and q. Thus, although this type of element is theoretically subject to the same stressing problems as the teledial, the very much reduced parameters undoubtedly help it to reach high ratings of the sort set out in Table 3 before running into stressing problems. The fact that a different graphite with lower coefficient of thermal expansion is being used will also mitigate the problem.

On the other hand the stresses due to heat fluxes from the compacts have to be superimposed upon those developed in the block as a whole due, for instance, to cross-block temperature and flux tilts. A detailed stress analysis is necessary to define accurately the limiting conditions but, on the basis that the design point is not limiting, Table 3 includes some estimated costs.

It may be assumed that since the graphite fuel pin components had been deleted in this type of design the fuel costs may decrease by as much as 20% and therefore affect generation costs accordingly. On the other hand tight ligament spacing may lead to higher reject rates which counter-balance the advantage. The capital and generation costs set out in Table 3 therefore make the assumption that in addition to

stressing patterns and temperature limits being satisfactory, the fuel manufacturing costs are broadly in line with what would be expected from the other types of designs.

Tubular Interacting Design

In an attempt to side-step the problem of gap temperature differences altogether, the tubular design can allow the compact to shrink on to the inner graphite sleeve. The outer gap then tends to be stabilised in its dimensions and in any case in carrying a smaller heat flux. By use of this device it is possible to restrict fuel maximum temperatures by 100°C compared with a non-interacting tubular design of the same rating. Furthermore, the uncertainties on wall temperatures are now reduced. In exchange for stabilising of the temperature problem however, one now has to take account of the stresses induced in the compact during operation. These arise as a result of the differential shrinkage between compact and graphite; but shrinkage rates and the effect of compact creep are such that the peak stresses occur for a relatively short period of time. Calculations show that they are limited to values of approximately $0.4 \times \text{UTS}$ of the compact. This sort of stressing problem is indicated in Fig 8 and Fig 9, which show the build-up of nominal stress and also the effect of various uncertainties. Note that the compact stress is again dependent on differential shrinkage rates and is not affected by the fuel rating.

It is also desirable to show that any reactor transient does not produce temperature distribution in the graphite wall and compact which can overstress the compact beyond the normal operating levels discussed above. This basically depends on coefficients of thermal expansion which are fairly well established and so far do not appear to be limiting.

It is possible to envisage further development of an interacting design in which the compact is replaced by a fuel bed which is cast integrally into the graphite sleeves. This type of design would remove

gap temperature differentials entirely but once again there would be the problem of interaction between the compact and the graphite. The state of development of this approach is not yet clear and further work requires to be done on it before it could be adopted for a prototype reactor.

COMPARISON OF DESIGN

All the above designs are capable of being built at the present time and of using the materials currently available in the UK. All of them are subject to material limits of one sort or another (fuel temperature, graphite or compact stress) and Table 3 attempts to quantify the resultant capital and generation costs for designs at the limiting parameter. These costs should not be regarded as absolutely accurate since adjustments to the design point could produce different numbers. Nevertheless, it presents a reasonable guide for comparison of the various technically feasible designs and probably gives more or less the correct economic ranking.

However, a selection of element cannot be based solely on economic criteria alone and a degree of engineering judgement needs to be injected, particularly to weigh the uncertainties which may be associated with each design.

As pointed out in the Coated Particle Design section, there are likely to be few uncertainties in particle design apart from endurance at elevated temperatures of 1400°C and beyond.

The Design of Fuel Compact section, in discussing the compact design, shows that here also there is a high degree of confidence in the ability to manufacture compacts of the required standard. The main task is to fill in irradiation data uncertainties and confirm the shrinkage predictions.

The graphite which has been the basis of UK work up to the present, is a fine-grained, high density isotropic graphite based on Gilsocarbon. This is very well understood in terms of its irradiation performance and mechanical properties. On the other hand, it is not matched to

the compact in terms of shrinkages at the point of interest. There is, therefore, a case for investigating graphites with slightly different characteristics (higher shrinkage to match gaps/lower coefficient of thermal expansion to reduce thermal stresses) and it is hoped that this can be done before 1973. If it can be done, it will raise the limits of all the designs and although it is not likely to alter their economic rankings it will relieve the pressure on design margins particularly those concerned with graphite and compact stresses e.g. teledial and tubular interacting. Such work, however, is not essential and designs can be produced within the current materials restrictions.

The most important uncertainty affecting element choice therefore revolves around the particle endurance at temperatures of the order of 1400°C and above and this therefore puts the main question mark against the rod type elements which must operate at these temperature limits to attain reasonable core ratings.

The apparent simplicity of rod designs may therefore be misleading and the selection of element probably needs to be made between:

- (a) the teledial
- (b) the tubular interacting and
- (c) the integral block

as being the elements with most conservative fuel temperatures.

The teledial is economically less attractive but its (graphite thermal stress) limitation may be deemed to be the least worrying of all since it is unlikely to lead to catastrophic failure of the element.

The tubular interacting design is economically the most attractive and probably represents the ultimate as a long-term development. In this respect, it would also simplify the overall development programme if it was adopted as the element for the first station. The question mark over compact stresses depends upon accuracy of differential shrinkage data and, for transient considerations, coefficients of thermal expansion. If current data is confirmed (or improved by use of a different graphite) by 1973 there is no problem and it is this

judgement which must be made for it to be adopted for a first-off station.

The integral block design represents an alternative which certainly must be viable at the ratings being currently employed and, since it is similar to the teledial in principle, is to be preferred to the latter by reason of its rather better economics. However, it is not quite so promising as the tubular interacting element, particularly in the long term.

On balance, we conclude that the tubular interacting element is to be preferred as the design of element for a first-off station. The integral block would be a second choice and, indeed, there is something to be said for the view that each is a back-up for the other.

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Table 1. Parameters of Coatings in BelgoNucleaire 800 μm
Kernel Reference Particle

	Reference particle
<u>Kernel</u>	
composition	UO_2
density	90% T.D.
diameter	$770\mu\text{m} \pm 60\mu\text{m}$
<u>Coatings</u>	
PyC buffer layer	
thickness	$33\mu\text{m}$
density	1.05 g/cc
PyC transition layer	
thickness	$30\mu\text{m}$
density	$1.0 \text{ to } 1.7 \text{ g/cc}$
Inner HD PyC layer	
thickness	$30\mu\text{m}$
density	1.8 g/cc
BAF	$1.0 \text{ to } 1.1$
SiC layer	
thickness	$35\mu\text{m}$
density	3.2 g/cc
Outer HD PyC layer	
thickness	$40\mu\text{m}$
density	1.8 g/cc
BAF	$1.0 \text{ to } 1.1$

Table 2. Breakdown of Costs for Two Different Tubular Interacting
Designs at 1.0 and 0.8 g/cm^3

		A		B	
		8.8		11.0	
Fuel pin area	cm^2				
HMD	g/cc	1.0	0.8	1.0	0.8
Nc/Nu		260	320	230	285
Max. channel power	kW	700	700	700	700
Construction cost	$\text{\$/kW}$	2.2	2.4	1.1	1.2
Initial uranium	$\text{\$/kW}$	1.1	0.3	2.3	1.1
Initial fabrication	$\text{\$/kW}$	0.9	0.8	1.1	1.1
Total capital	$\text{\$/kW}$	4.2	3.5	4.5	3.4
Replacement uranium	$\text{\$/kW}$	-1.0	0	-1.4	-1.1
Replacement fabrication	$\text{\$/kW}$	-0.3	0.8	-0.9	0.2
Other running cost	$\text{\$/kW}$	0.1	0.1	0.1	0.1
Total generation	$\text{\$/kW}$	3.0	4.4	2.3	2.6

This table sets out the detailed make-up of cost components for capital and generation costs of two typical designs. The costs in $\text{\$/kW}$ are present worth values and are shown as differentials from suitable datum costs.

Table 3. Typical Design Points for various Element Types ⁽¹⁾

		<u>Hollow Rod</u>	<u>Trefoil</u>	<u>Tubular Non- Interacting</u>	<u>Tubular Interacting</u>	<u>Teledial 8-hole design</u>	<u>Integral Block</u>
<u>Fuel Compact</u>							
Inside diameter	mm	25.4	-	39.5	39.5	-	-
Outside diameter	mm	42	19.0	54.4	54.4	12.5	12.7
<u>Pin Design</u>							
Inside diameter	mm	-	-	29.5	29.5	24.6	-
Outside diameter	mm	52	29.0	64.4	64.4	65.6	-
Length	mm	500	500	500	500	500	793
<u>Block Design</u>							
Pins per block		36	108	36	36	36	210
Channel diameter	mm	63.4	41.0	74.8	74.8	76.6	15.9 (X 108 off)
Dimension across flats	mm	377	375	422	422	436	361
Length	mm	1000	1000	1000	1000	1000	793
<u>Temperatures</u>							
Nominal fuel temperatures ⁽²⁾	°C	1250	1210	1185	1085	1123	Not known
Nominal max sheath temperature	°C	950	878	920	920	910	Not known
<u>Ratings</u>							
Mean fuel rating	MW/Te	43.7	83.50	77.5	77.5	81.5 (5)	54.4
Mean core rating	MW/m ³	4.73	8.60	8.40	8.40	8.65	6.11
<u>Limiting feature</u> ⁽³⁾		Fuel temp	Fuel temp	Graphite stress	Compact stress	Graphite stress	Probably graphite stressing
<u>Costs</u> ⁽⁴⁾							
Capital cost differential	%	+10	0	0	0	+5	+4
Generation cost differential	%	+6	+2	0	0	+3.3	0

- Notes
- (1) These design points are typical only, they are probably close to optimum within the particular limits set in each case
 - (2) Fuel temperatures are nominal maximum and do not include random and systematic effects. These can raise fuel temperatures by up to about 150°C
 - (3) Graphite and compact stresses are set at between 0.4 and 0.5 x UTS as a limit. Fuel temperature maximum is assumed to be 1400°C peak random with 1200°C a more desirable limit.
 - (4) Costs are referred to an arbitrary datum
 - (5) Graphite stresses too high in this design and downrating to approximately 55 MW/Te is recommended in which case, costs are as below

Fig 1 Particle Stress History

Fig 2 Particle Burnup as a Function of Kernel Density and Buffer Layer Thickness

FUEL TYPE	1	2	3
DENSITY g/cc	HR	TI	TI
Nc/Nu	1.0	1.0	0.8
	236	260	260

Fig 3 Graph of Cost v Mean Irradiation

FUEL TYPE T I
 DENSITY g/cc 1.0
 PIN AREA CM² 11.0

Fig 4 Graph of Cost v Moderator to Fuel Ratio

Fig 5 Drawing of 5 Element types

Fig 6 Hollow Rod Stress Pattern

Fig 7 Teledial Stress Pattern

Fig 8 Tubular Interacting Stress Patterns

COMPACT HOOP STRESS AT BORE DUE TO INTERACTION AT 60% CORE HEIGHT

DOUBLING RATING INCREASES MAX STRESS 160 PSI 17%
 REDUCING GRAPHITE THICKNESS BY 20%
 REDUCES STRESS BY 100 PSI 10%
 REDUCING RATING BY 10%
 REDUCES STRESS BY 15 PSI 2%
 WORSENING SHRINKAGES BY 20%
 INCREASES STRESS BY 140 PSI 14%
 DECREASING COLD CLEARANCE BY 80 μ
 INCREASES STRESS BY 100 PSI 10%
 INCREASING COLD CLEARANCE BY 80 μ
 DECREASES STRESS BY 150 PSI 16%

Fig 9 Sensitivity of Tubular Interacting Stresses
to Parametric Changes

DISCUSSION

H. B. Stewart: We gave some attention to tensile stresses that might arise in the fuel compact for the tubular interacting fuel design. Would the development of stress cracking in some of the compacts present any problems?

D. J. Merrett: It is obviously desirable to avoid compact fracture and this is why we would like to have high strength compacts so that the possibility of fracture is very low. If, however, there was a fracture, there are two aspects to the problem. Firstly, the temperature of the compact might increase marginally to a limit which would approach that of a tubular noninteracting design. From Table III it can be derived that this increase in temperature would be limited and is unlikely to lead to any rapid deterioration in particle integrity, particularly with the very low fuel temperatures chosen for this design.

Secondly, the fracture might mechanically break a particle. More evidence is required on this. Some recent tests have shown a few broken particles when a compact is fractured. These fracture tests were by pneumatic disintegration (internal pressure) of the compact and may be unrepresentative. A few particle failures, however, would not be serious.

R. A. U. Huddle: I would like to comment on three points. Firstly, concerning the use of peak nominal. We are interested in a failure of say 10^{-3} fraction of particles. It is, therefore, important to consider not the nominal but the peak random as it is about 10^{-3} fraction of fuel that is at or above this temperature. In comparison, therefore, the only relevant parameter is peak random as the difference between the nominal and random figures varies greatly for the different designs.

Secondly, regarding the necessity for a high strength compact. It is beyond my comprehension why one needs high strength particularly for the teledial and any other noninteracting design. In the interacting design, it is the creep that matters and this is a very difficult and time consuming parameter to evaluate. Let's forget high strength compacts as it just leads us into very serious trouble in fabrication.

Lastly, I disagree entirely with the statement that fracture through a compact will certainly break particles. It is just contrary to all our experience at Dragon.

D. J. Merrett: 1. We agree that peak random temperatures are important. Parametric surveys have to be conducted in terms of nominal temperatures; in picking a preferred design point, the peak random must be taken

into account. 2. High strength compacts are not strictly necessary for teledial or noninteracting designs, and matrix density could be reduced although one should not lose sight of thermal conductivity advantages. 3. Creep is not a key parameter. Our stress calculations have assumed compact creep data the same as the graphite tube. The situation is likely to be better than this; the danger of fracture might come from strain limitations. 4. High strength compacts can be fabricated without "very serious trouble", as is outlined in the next paper. 5. As pointed out to Dr. Stewart, more work is needed in the aspect of particle fracture on breakage of the compact. On reflection, I agree that Dragon evidence is more applicable.

E. Smith: One must be careful in applying the material data deviations statistically to deduce allowable design stress limitations since some changes (for instance, modules and strength) are related changes.

Will the speaker indicate the cost penalties of the teledial design if graphite stresses are not considered to be limiting.

D. J. Merrett: In your first point, I agree. In regard to your question, if there were no stressing limit, a close-to-optimum teledial design would be marginally more expensive than a tubular design in total generation cost, to the extent of perhaps one pound per kilowatt. This stems basically from slightly worse N_c/N_u ratios and slightly higher core pressure drops which have blower consumption penalties.

H. Gütman: The comparison between the different designs depends very much on the underlying assumptions. In your comparison the teledial design is rather heavily penalized due to shutdown stress limitations, as you say. I think that our philosophies are disagreeing on this point and my paper will show a different outcome of such a comparison. May I ask you what percentage of the ultimate strength you have assumed as design limit for your shutdown stresses?

P. U. Fischer: With bonded rods considerably higher metal loadings could be reached. How would these affect the comparison of the various fuel element designs you presented?

D. J. Merrett: With our current design and one gram per cc compacts it is just about possible to reach optimum N_c/N_u ratios of around 240. Lower heavy metal densities become more difficult to accommodate, and depending on design details there may be overall generating cost penalties approaching a typical maximum of about 1.5 pound per kilowatt. The use of 1.5 grams per cc compact would obviously ease the problem but would not lead to proportional savings.

FUEL DEVELOPMENT FOR A LOW ENRICHED HTR

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Abstract

The approach pursued for the design and development of a HTR fuel was outlined in a paper presented at the Julich Symposium held in October 1968¹. The present contribution describes the continuation of the development work, the design aspects being covered in another paper² at the present symposium.

The optimum porosity of the kernel requires further investigation, since there is a discrepancy between the design predictions and some irradiation results. Fabrication techniques have therefore been developed to cover the 85 - 95 % TD range. The kernel properties within this range are compared.

The coating layers follow the scheme proposed by the Dragon Project. Exemplative results are given on the variations of the dimensions and the properties within a batch and from batch to batch.

Various approaches to the manufacture of fuel rods are presently being pursued at Mol. Some results of fabrication trials and properties investigations are reported. The influence of manufacturing features may be of major importance in the selection of a fuel rod design. The cases of tubular and teledial rods are outlined.

An analysis of available corrosion data has been realized. The influence on the design of fuel pins is commented.

Some unusual irradiation features are reported, as an illustration of the programme pursued.

INTRODUCTION

The Belgian nuclear fuel industry has coordinated its actions within the "Groupement Général du Combustible Nucléaire" (G.G.C.N.). Three companies : "BELGONUCLEAIRE" (BN), "Métallurgie Hoboken" and "Métallurgie et Mécanique Nucléaires" (MMN) are performing activities in the field of HTR fuel.

The very close cooperation with the Belgian Study Centre for Nuclear Energy (C.E.N. - S.C.K.) and the continuous contacts with the Dragon Project have assured the availability of the necessary knowhow. A good example of this ideal environment was the development and the manufacture, in 1964, of plutonium base coated particles, the good behaviour of which was demonstrated up to extreme burn-up (60 % fima up to 1200°C)³ and temperature conditions (1850°C up to 20 % fima)⁴.

In 1967 the decision has been taken to concentrate in Belgium on the industrial development of HTR prismatic fuel. BELGONUCLEAIRE, a member of Inter Nuclear, has the leadership of this Belgian program and insures the complementarity to the fuel development and design activities of Inter Nuclear.

The fuel elements considered are described and their relative merits compared in two other papers to this meeting. The present paper will therefore concentrate on one reference design shortly described hereafter. But mentions will be made of the others whenever relevant. For simplicity reasons, the nomenclature used is summarized in Table 1.

A typical prismatic fuel element is a 1000 mm long hexagonal graphite brick 420 mm across flats, with eighteen 75 mm diameter coolant channels. Each channel contains two annular fuel rods 64 mm OD, 29 mm ID, by 500 mm long, cooled both internally and externally. The fuel elements stack to form a free standing column of five or six elements plus reflector and thermal shield units. Beside providing a structural support for the rods, the bricks provide the graphite moderator, so that the whole core is replaceable.

A high uranium loading (density) and a long fuel life (MWD/TeU) are desirable to achieve low fuel cycle costs⁵. These considerations have led to the choice of a fuel kernel of 800 microns diameter and 80 - 90 % TD uranium dioxide, surrounded by a triplex coating of pyrocarbon and silicon carbide to give a nominal particle diameter of 1100 microns. The indication of the behaviour of such particles in a wide range of operating conditions is part of the Dragon Program⁶.

The fuel particles are embedded in a graphite matrix and packed into the annular graphite sleeves which serve the dual function of containing the particles and protecting them from corrosion by impurities in the coolant. The fuel rods are manufactured separately from the hexagonal brick, which is convenient for quality control and avoids the rejection of a complete fuel element as a result of a defect in one rod. We are presently using a fine grain isotropic graphite for the sleeves with a trepanned fuel annulus to provide an integral "closure" at the bottom end. The top closure is provided by a graphite annular end cap.

OPERATING CHARACTERISTICS OF HTR FUEL

Based on the assessment of various fuel element types 1, 5, the operating characteristics are summarized in Tables 2 and 3.

Table 1. Types of HTR Fuel Elements¹

Type	Symbol	Characteristics
Hollow rod	HR	rods cooled externally only ; annular fuel region
Tubular	Tu	annular rods cooled from both sides
Trefoil	Tr	rods cooled externally only ; three rods per coolant channel ; plain fuel sticks
Teledial	Te	annular rods with plain fuel sticks
Integral block	Bl	plain fuel sticks, incorporated directly in the block

Table 2. Desirable Fuel Endurance Limits

Operating temperature	1300° C (1400° C for HR)
Fraction of defective particles	
- start of life	10 ⁻⁵
- end of life	10 ⁻³
Minimum thermal conductivity	{ 0.2 - 0.3 W/cm°C for HR and Tr { 0.10 - 0.13 W/cm°C for Tu { 0.03 - 0.14 W/cm°C for Te and Bl
Heavy metal loading	1.0 g U/cm ³ (less for Bl)
Heat rating	0.3 W/particle (0.2 W/p for HR)
Gas release	
- R/B ratio	10 ⁻⁵
Burn-up	100 GWd/t (80 GWd/t for HR)
Residence time	800 d (1300 d for HR)

Table 3. Desirable Graphite Endurance Limits

Graphite density	1.8 g/cm ³
Mean temperature	800° C
Peak surface temperature	1050° C
Thermal conductivity	1.2 W/cm° C
Maximum thermal expansion coefficient	10 ⁻⁶ / °C
Four point bend strength	200 kg/cm ²

REFERENCE SPECIFICATIONS AND MANUFACTURING RESULTS

In a typical HTR power plant the fuel is subdivided into 10^{10} containers; this shows that HTR fuel is a perfect case for statistical treatments.

Specifications can be issued presently to use as a reference; but industry, in the field of reactor design and of fuel manufacture, needs to check how close the tolerances can be kept and what is the cost of keeping and controlling various tolerance levels.

Most of the paper will therefore be devoted to our contribution to this aspect. It must be understood that it is the present stage and by no means the final one or the economical optimum.

KERNEL

Fuel material

The uranium dioxide has been preferred to the carbide for the following reasons :

- the dimensional stability is better at high burn-ups ⁷,
- the oxide will not intrude into the pyrolytic carbon coatings under normal temperature gradients ⁶,
- the uranium contamination in the coating layers can be kept to a much lower level during fabrication, which results in a lower fission product release,
- the oxide is much easier to handle during the first fabrication steps.

The oxide can also be applied for the thorium cycle; it is a must in the case of plutonium particles, as was demonstrated by the Joint C.E.N. - BELGONUCLEAIRE Plutonium Group, in collaboration with DRAGON ⁸.

Kernel size

The fabrication cost of the coated particles decreases when the kernel size increases up to a kernel diameter around 900 microns. For higher kernel sizes, the fabrication cost does not decrease any more.

The particle size influences also the heavy metal loading and the fuel cycle cost ⁵. For a same volume loading of coated particles, the heavy metal loading decreases by 30 % when the kernel size is passing from 800 to 550 microns; compensating this by an increase of volume loading is impossible in most cases (Cf Par. 6).

The coating operation presents some problems for large sizes, it is for the moment safe to consider 900 microns as a limit for the kernel diameter.

For these reasons 800 microns was selected as reference diameter.

The size distribution within a batch and the reproducibility from batch to batch are function of the manufacturing technique. Typical results are reported in Table 4, for the routine powder agglomeration technique (Fig. 1) and a representative gel precipitation process. While the distribution is gaussian in the latter, it is not in the former (Fig. 4).

The significance of a close tolerance is unknown. It was reported earlier that no correlation seemed to exist between the kernel diameter and the coating thickness on particles coated in a same batch. Recent information⁹ seemed to show the contrary. New investigations tend to confirm a likely compensating correlation during the coating steps with thicker layers on the larger particles.

Density

The optimization of the average density is another field for further investigation. Undoubtedly high porosities correspond to more conservatively designed particles. However, two factors affect adversely the lower density kernels, all other things being equal : a lower strength (leading to a higher contamination of the coating) and an increased number of cracked particles in a compact (due to the higher volume loading required to reach a given heavy metal loading).

To illustrate this last statement, Table 5 summarizes the result of a parametric survey⁷ performed with the COCONUT code : the buffer thickness was varied to such an extent that the stress level in the SiC layer was the same, at the end of life, for all the kernel porosities.

Table 4. Kernel Characteristics

Manufacture	Powder agglomeration	Gel precipitation
Diameter (μ)		
- average	761	758
- reproducibility of the average of 400 particles	- 14 + 6	- 6 + 4
Density (g/cm^3)		
- average	10.0	10.8
- reproducibility (on Hg density samples)	± 0.2	± 0.1
O/U		
- average	1.985	2.005
- reproducibility	± 0.005	± 0.0035

Table 5. Influence of the Kernel Density (% TD)
on the Volume Loading (v/o) of Coated Particles in the Fuel Region

Kernel	800 μ
Coating	- buffer variable
	- transition and HDI PyC ... 40 μ
	- SiC 30 μ
	- outer PyC 50 μ
HML	1.0 g U/cm ³
Temperature	1200° C
Burn-up	10 % fima
Fluence	4 x 10 ²¹ DNE i.e. 7 x 10 ²¹ n cm ⁻² (> 0.1MeV)

Buffer density (g/cm ³)	1.0	1.2
↓ Kernel density (% TD)		
70	40	39
80	35	34
85	33	33
90	31	32
95	30	31
98	30	32

On the powder metallurgy route, the main effort has been devoted to assess the possible use of standard materials and equipment so that the preparation of kernels can fit in a by-pass line of a pellet fabrication plant, the preparation of the green spheres replacing the conditioning of the powder and the pelletizing. With ceramic powder of normal specifications (3 to 4 m²/g) obtained from UF₆, the technique produces densities between 90 (± 1.5) % TD and 95 (± 1.0) % TD. As a good compromise between the considerations developed in the previous paragraph and a desire to keep a conservative attitude, 90 % TD has been selected as reference.

At this stage, it was thought advisable to cover the range from 85 to 95 % TD. Therefore, desactivation techniques have been studied to reduce the sinterability of the powders. Both hydrogen and CO₂ treatments have been investigated. A CO₂ treatment at 800° C reduces the surface area to 1.5 m²/g whereas the hydrogen treatment at 1350° C to 0.5 m²/g. These techniques allow to cover the whole density range without modifying the powder agglomeration and the sintering steps. Figures 2 and 3 show a metallographic cross section of representative kernels at 83 and 90 % TD.

Oxygen to U ratio

The chemical reaction between fuel and pyrocarbon produces carbon monoxide. Figure 5 shows the CO equilibrium pressure for the ternary system U-O-C calculated as a function of temperature using known thermodynamic data.

An excess of oxygen into the kernel can create a very important internal pressure and great care should therefore be taken to condition the kernels prior to coating in order to reach the stoichiometric or a substoichiometric state.

Table 6 includes results on kernels made by the wet and dry process. The figures relate to the products as sintered and after a reduction treatment at high temperature. It may be noticed that within a process there is a slight tendency for the O/U ratio to increase with porosity.

Table 6. O/M Ratios (Analytical Accuracy : ± 0.005)

Route	Gel precipitation		Powder agglomeration	
Density	As sintered	Treated	As sintered	Treated
70 %	2.013	2.01	n.a.	n.a.
80 %	2.010	2.008	1.990	1.980
85 %	2.005	2.004	1.994	1.980
90 %	2.004	2.003	1.985	1.980
95 %	2.003	2.003	1.980	1.980
99 %	2.003	2.003	n.a.	n.a.

Table 7. Isotopic Analysis and Activities of Uranium used for the Preparation of the UO_2 Particles

Isotope	%	Spec. activity (curie/g)	Partial activity (curie/g)
233	0	$948 \cdot 10^{-3}$	
234	0.020	$6.2 \cdot 10^{-3}$	$12.4 \cdot 10^{-7}$
235	4.715	$2.14 \cdot 10^{-6}$	$10.09 \cdot 10^{-7}$
236	0.036	$6.3 \cdot 10^{-5}$	$21.42 \cdot 10^{-7}$
238	95.229	$3.33 \cdot 10^{-7}$	$3.17 \cdot 10^{-7}$
Specific activity of the mixture (in curie)			$47.08 \cdot 10^{-7}$
d/sec, g			$17.42 \cdot 10^{-4}$

COATED PARTICLE

Specifications

The standard Dragon coating has been retained with specifications adapted to our reference kernel. The purpose of the various layers has been described in the literature. Only some comments will be given to the successive layers :

- Buffer_layer :

A density in the range of 1.0 to 1.1 g/cm³ was selected. For the reference kernel, a buffer thickness of 35 microns is necessary; this is above the minimum thickness which is required for protection against fission products recoils.

- Transition_layer :

With the standard coating equipment, the necessary thickness is 30 microns.

- Inner_high_density_PyC_layer :

Although COCONUT calculations indicate an advantage to reduce the thickness, 30 microns remains the reference value.

- SiC_layer :

For the mechanical behaviour, the thickness should be minimum. Since the main purpose is to act as a barrier for solid fission products, its thickness is kept fairly large at present.

- Outer_high_density_PyC_layer :

As far as is known of the creep coefficient, it is necessary for this layer to be dense isotropic and rather thick.

Controls

The controls are an important but costly part of the coating process. Some comments are worth mentioning in the present paper.

Control Procedures

All controls are carried out in accordance with the standard Dragon procedure. Only slight differences can be noticed on the following controls :

- layer thickness by V-measurements.

The figures relate to the mean value on four times 100 particles. The particles are put in a V-shaped holder and the total length is measured with a micrometer (± 0.01 mm). An accuracy of $\pm 5 \mu\text{m}$ can easily be obtained.

- layer thickness by radiography.

A radiographic picture of 100 particles is taken by a monochromatic beam. A WC sphere of well known diameter is simultaneously radiographed as a standard. The radiographic film is measured on a microscope. In order to improve the contrast of the outer carbon layer, a Ni plating is applied over the coated particles.

- Densities.

The density of the inner and outer HDI-PyC layers are controlled by means of the "sink and float" technique on small discs. The geometrical densities over the total layer thickness is calculated on the basis of the mercury density of the coated particles.

- Fraction of visible core.

Four 2.5 g samples are controlled on an alpha-counter. This contamination is translated into fraction of the core, using the specific activity of the kernels. As an example, Table 7 details the calculations of the specific activity of the kernel batch, utilized in the experiments described on page 18.

- Gas content.

A sample of 2 g of coated particles is heated at 2200°C for one hour and the amount of released gas is measured. This test serves simultaneously as a hot run test.

Control Plans.

During the trial runs, each coating layer is controlled individually until all the specifications are met. Afterwards only a composite sample of four batches is fully controlled. The next step will be to control only one representative sample out of 10 coating batches, provided the furnace is fully monitored to detect abnormal coating operations.

During the pre-production period (trial runs), the controls performed include, on each individual layer :

- the layer density, by three techniques (mercury immersion, disks and geometrical density) ;
- the layer thickness, by two techniques : the standard micro-radiography technique and measurements mentioned in the previous paragraph ;
- ceramographic cross-sections ;
- microprobe analysis to check the Si/C ratio ;
- uranium content (on the final product only) .

For the production batches, the controls effected on every batch include sieving and shape separation on the fuel batch and on samples, the sieve size distribution, the particle density, the particle diameter by V-slot measurements and the determination of the contamination. The supplementary controls on a composite sample on four or ten batches include the determination of the layer thickness by microradiography, the outer HDI PyC layer density by disk measurements, a ceramographic section, X-ray analyses to check the presence of carbides and the B.A.F., the microprobe analysis of the Si/C ratio and the determination of the uranium content and the impurities.

It is foreseen that the frequency of the controls could be reduced upon later industrial scale-up.

Reproducibility Experiments

At regular intervals, series of coating runs are performed, with the purpose of gathering statistical information. These runs are usually serving the simultaneous purpose to produce large amounts of uniform materials for other parts of the program (development of the consolidation technique, neutron physics experiments, etc...) .

The results of such a campaign, performed in 1969, will be described hereafter. The required target specifications are given in Table 8. A standard 80 mm coating furnace was utilized and the coatings were performed on 1 kg batches.

Table 8. Target Coating Specifications

Layer	Thickness (μ)		Density (g/cm ³)	B.A.F.	Crys- tallite size
	average	stand. dev.			
Porous PyC	30 $\begin{smallmatrix} + 5 \\ - 0 \end{smallmatrix}$	5	1.0 $\begin{smallmatrix} + 0.1 \\ - 0.0 \end{smallmatrix}$	-	-
Transition PyC	30	-	1.6 $\begin{smallmatrix} + 0.1 \\ - 0.0 \end{smallmatrix}$	-	-
Inner HDI PyC	30 $\pm$ 5	5	1.8 $\pm$ 0.05	1.00 - 1.08	70
SiC	40 $\pm$ 5	4	3.2 $\begin{smallmatrix} + 0.0 \\ - 0.02 \end{smallmatrix}$	-	-
Outer HDI PyC	30 $\pm$ 5	5	1.8 $\pm$ 0.05	1.00 - 1.08	70
Total Thickn.	160 $\pm$ 10	10	-	-	-

Table 9 gives the coating conditions which were found to match the required specifications.

After these coating conditions were fixed, a series of 33 runs was performed. Table 10 gives the results of the in-line controls carried out on each batch.

On ten batches the uranium content, X-ray and gas analysis were carried out. A mean value of 1.06 % was found for the uranium loss with a range between 0.48 and 1.99 % . X-ray analysis showed that there was no carbide on the UO_2 kernels. The UO_2 parameter was 5.4693 Å.

The results of the gas analysis are given in Table 11. Figure 6 gives a micrograph of a representative batch.

Present State of the Art

The results given here , although incomplete, show that a very clean and thermally stable (Table 11) product can be made in a routine production.

The rather wide scatter of the coating weights (Table 10) are due to industrial flow meters used for the production; this scatter seems to come equally from the coating thickness and density. The detailed analysis of the individual layers (measured on radiographs) is then necessary to decide whether more elaborate flow meters are necessary in an industrial production.

On the basis of such results and the irradiation behaviour data, the specifications are regularly revised. The current coating specifications for our reference kernel are given in Table 12.

Table 9. Coating Conditions used for the Reproducibility Tests

Layer	Temp. °C	Time min.	Total l/min.	Central nozzle (%)						Outer nozzle (%)		
				C ₂ H ₂	CH ₄	CO	A	direct H ₂	via Sil.	Silane	CO	A
Porous	1450	2	33.3	40.2	-	13.3	-	-	-	-	46.5	-
	(1500	2	32.6	7.7	-	44.9	-	-	-	-	47.4	-
Transition)	1600-											
	(1700	17	26.5	-	18.5	30.8	-	-	-	-	50.7	-
I. PyC	1800-											
SiC	{ 1950	23	22.3	-	21.1	-	22.9	21.4	-	-	-	34.6
	{ 1550	45	39.3	-	-	-	-	30.8	34.1	4.6	-	30.5
O. PyC	{ 1800-											
	{ 1950	33	22.3	-	19.3	-	22.9	19.2	-	-	-	38.6

Table 10. Coating results obtained on 33 batches of 4.7 enriched UO₂ (in-line controls).

		Unit	Mean value	Range	Standard deviation
P a r t i c l e s	<u>Weight of one</u>	mg	3.28	3.21 - 3.33	0.04
	<u>Diameter</u>				
	V-slot	μm	1074	1064 - 1088	5.7
	<u>Densities</u>				
	a) Bulk	g/cm ³	2.80	2.73 - 2.87	0.034
	b) Tap	"	3.09	3.00 - 3.16	0.04
	c) Geometrical	"	5.05	4.94 - 5.12	0.04
	d) Mercury meas.	"	4.97	4.85 - 5.09	0.06
C o a t i n g	<u>FVC</u>				
	(fraction visible core)		6.9.10 ⁻⁷	to 1.65.10 ⁻⁸ 3.8 . 10 ⁻⁶	9.1.10 ⁻⁷
	<u>Total U - content</u>				
	(calculated)	w/o	66.57	65.4 - 68.53	0.75
	<u>Total thickness</u>				
	V-slot	μm	158	153 - 166	2.99
	<u>Total layer density</u>	g/cm ³			
	a) mercury	-	1.85	1.74 - 1.91	0.039
C o a t i n g	b) geometrical	-	1.89	1.75 - 1.95	0.051
	<u>Coating weight per 1000 g particles</u>	g	320.6	284 - 343.2	14.26

Table 11. Results of the Gas Analysis
(heat treatment : 1 hr at 2200°C)

	Unit	Mean value	Range
<u>Contamination level</u>			
- before heat treat.	fVC	$5.8 \cdot 10^{-7}$	$6.9 \cdot 10^{-8}$ to $2 \cdot 10^{-6}$
- after heat treat.	fVC	$3.5 \cdot 10^{-7}$	$1.1 \cdot 10^{-8}$ to $5.6 \cdot 10^{-7}$
<u>Amount of gas</u>			
- first 10 min.	$\mu\text{l/NTP}$	11.21	14.5 - 6.8
- after 60 min.	$\mu\text{l/NTP}$	1.8	2.8 - ≤ 0.5

Table 12. Current Specifications for the Reference Fuel

Layer	Thickness (μ)	Density (g/cm^3)
Buffer	$33 \begin{smallmatrix} + 6 \\ - 1 \end{smallmatrix}$	1.0 ± 0.08
Transition	30 ± 5	1.6 - 1.7
Inner PyC	30 ± 5	1.8 ± 0.05
Total PyC	95 ± 10	
SiC	36 ± 5	$3.2 \begin{smallmatrix} + 0.005 \\ - 0.02 \end{smallmatrix}$
Outer PyC	37 ± 5	1.8 ± 0.05
Total coating	170 ± 10	-

Table 13. Typical Characteristics of the Fuel Bodies

Type		Pressed compact	Cpct pres-sed in situ	Consolidt. compact
- CP loading (max.)	v/o	35	40	50
- HML ^a (max.)	g U/cm^3	1.0 ± 0.05	1.3	1.6
- matrix density	g/cm^3	1.6 - 1.7	1.4	1.2
- graphite content of the matrix	%	95	92	85
- contamination	fVC	10^{-6}	10^{-6}	10^{-6}
- fraction of broken particles	-	2×10^{-4}	10^{-4}	5×10^{-5}
- nominal gap				
fuel/outer sleeve	μ	250	130	100
fuel/inner sleeve	μ	250	40	10
- tolerance on gap ^b				
fuel/outer sleeve	μ	110	100	40
fuel/inner sleeve	μ	90	13	negl.
- thermal conductiv.	$\text{W/cm}^{\circ}\text{C}$	0.2	0.16	0.06

^a with a kernel 800 μ diameter and 90 % TD and a 170 μ thick coating
^b for a compact 40-50 mm OD and 32-39 mm ID.

FUEL RODS

The development work in Belgium is based on tubular fuel, as was outlined in a previous meeting. The advancement of fuel design and the delay necessary to demonstrate the behaviour of the consolidated fuel have led to abandon, for the reference fuel, the process described in 1968. The standard technique is now the fabrication of compacts (Fig. 7) and their introduction in the graphite sleeve (Fig. 8). The good behaviour of compacts has indeed been demonstrated by Dragon⁶.

Since this step is an important part of the fuel fabrication, the consolidation alternative and an intermediate one are still being pursued to a small scale. The main characteristics are given in Table 13.

Standard Compacts

The coated particles are overcoated by a resin impregnated graphite powder, by the powder agglomeration technique, to a nominal diameter of approximately 1600 microns. The density of the overcoating layer is $1.6 \pm 0.05 \text{ g/cm}^3$ after drying, in a rough vacuum, below the melting point of the resin.

The particles are then loaded in the dies on a weight basis and preheated to 100°C . The pressure is applied gradually and remains constant until a given height is reached. Upon completion of the cycle, the block is transferred to an ejection press for hot ejection.

The heat treatment requires a close control of the heating cycle up to 950°C and is performed in a continuous furnace in flowing inert gas. The cycle duration is 8 hours. The final treatment consists in a vacuum degassing in a batch furnace for 2 hours.

Various raw materials are being tested in the frame of our program (Fig. 9) .

Standard specifications follow roughly the figures given in Table 13. Representative results are given in Table 14; more statistical background is needed in this field, to assess the economic implications of the tolerances.

In Situ Compaction

The cost of the process would be decreased and the dimensional tolerances eased if the compaction could be realized directly in the graphite sleeve. Preliminary trials have indicated the advantages and drawbacks of this technique, among which one may indicate :

- the absence or the reduction to a low level of the gap between the fuel and graphite sleeve;
- the possibility of obtaining a higher HML (1.3 to 1.5 g U/cm³);
- the limitation of the matrix density to 1.3 to 1.4 g/cm³ matrix;
- the presence of radial cracks through the compacts; these cracks are however not affecting the integrity of the coated particles.

Advanced Consolidation Processes

A large effort has been devoted in 1967/68 to the selection of the consolidation process to obtain 1.5 to 1.6 g/cm³ HML. The only retained technique consists in the utilization of a high viscosity phenolic resin mixed with graphite powder and the introduction of the particles under influence of centrifugal forces.

After heat treatment and degassing, the matrix material contains 80 % graphite and has a density of 1.0 to 1.2 g/cm³ matrix, depending on HML.

The application of this technique, which allows also to incorporate directly the fuel into the graphite sleeve and has therefore the same advantages as mentioned in the previous paragraph, depends entirely on the irradiation behaviour of such fuel beds. Furthermore, its application is restricted to fuel concepts in which the rating is not limited by fuel temperature (e.g. Te and B1).

Furnace Overcoating

One of the costly steps of the compact fabrication is the overcoating. In order to decrease the cost, the powder agglomeration overcoating (Cf page 28) has been partially replaced by a low density PyC layer on top of the coating. This overcoating has the further advantage of protecting the particles from the force applied by the matrix during compression and allows to reach higher volume loadings.

The application of this technique depends entirely on the irradiation behaviour of this type of fuel beds.

CORROSION STUDIES

Corrosion studies by Dragon have been summarized and have shown that the concentration of water decreases rapidly within the cross-section of the graphite sheath surrounding the fuel over much of the sheath temperature range: a mathematical model has provided a means of calculating the profile of corrosion attack in depth beneath the sheath surface for given graphite properties and has outlined a simple means of correlating from 1 atm experimental results to the expected behaviour at reactor pressure.

TNPG have extended this work to areas not previously covered analytically :

- a) to provide a method of calculating the distribution of corrosion through the fuel rod cross-section with particular reference to the lower temperature fuel rods where gettering of the water is incomplete within the graphite sleeve section (Cf page 33);
- b) to provide a mathematical means for allowing for the changes in graphite properties that arise from corrosion in their effect on subsequent corrosion i.e.

- i) on the resultant depth of corrosion attack,
- ii) on the change in corrosion kinetics with burn off,
- iii) to compute i) and ii) for various pressures from the 1 atm pressure of experiments up to the reactor pressure case which cannot readily be examined experimentally (Cf pages 41 to 43).

Two reaction regimes are important :

- a) the chemical regime where the reaction rate is constant within the graphite section, diffusion replenishment of water always being rapid in relation to the point reaction rate;
- b) the 'in-pore diffusion' regime where diffusion replenishment is slow and reaction depletes the local water concentration at some zone within the graphite to a very small fraction of the surface water concentration, but where no water depletion occurs in the gas external to the geometric surface of the specimen.

Two-Zone (Sleeve and Fuel Matrix) Corrosion with Linear Temperature Gradients without Imposed Flow

In a reacting porous graphite medium Wicke has shown that the concentration of corrodent under diffusion conditions is governed by:

$$\frac{d^2 \gamma}{d\xi^2} = \varphi_o^2 \gamma \exp (2 \alpha \xi) \quad (1)$$

Transforming this using :

$$y = \frac{\varphi_o}{\alpha} \exp (\alpha \xi) \quad (2)$$

via a Bessel equation :

$$y^2 \frac{d^2 \gamma}{dy^2} + y \frac{d\gamma}{dy} - y^2 \gamma = 0 \quad (3)$$

has a solution :

$$\gamma(y) = A K_o(y) + B I_o(y) \quad (4)$$

Since the properties of the two regions are different separate solutions are obtained for the two regions and by applying four boundary conditions, four linear simultaneous equations are obtained : on applying the sheath/fuel boundary condition :

$$D_1 \left(\frac{d\gamma}{dy} \right)_{y=y_1} = D_2 \left(\frac{d\gamma}{dy} \right)_{y=y_1} \quad (5)$$

these simplify to :

$$\begin{cases} 0 & -K_1(y_2) & 0 & I_1(y_2) \end{cases} \begin{cases} A_1 \end{cases} \begin{cases} 0 \end{cases} \quad (6)$$

$$\begin{cases} D K_1(y_1) - K_1(y_1') - D I_1(y_1) I_1(y_1') \end{cases} \begin{cases} A_2 \end{cases} \begin{cases} 0 \end{cases} \quad (7)$$

$$\begin{cases} K_o(y_o) & 0 & I_o(y_o) & 0 \end{cases} \begin{cases} B_1 \end{cases} \begin{cases} 1 \end{cases} \quad (8)$$

$$\begin{cases} K_o(y_1) & -K_o(y_1') & I_o(y_1) & -I_o(y_1') \end{cases} \begin{cases} B_2 \end{cases} \begin{cases} 0 \end{cases} \quad (9)$$

Definition of Symbols :

γ = mole fraction of oxidant at a point, relative to its concentration at the outer surface,

ξ = distance of the particular point from outer surface, d, cm ,
wall thickness, L_1 , cm

φ_o = Thiele modulus or catalyst number,

$$= L_1 \frac{k_{mo} \rho_g}{D_{eff}},$$

k_{mo} = rate constant per gram for graphite chemical reaction rate
at T_o °K, $\text{cm}^3 \text{g}^{-1} \text{sec}^{-1}$,

ρ_g = apparent density of graphite, g cm^{-3} ,

D_o = diffusion coefficient of water in STP helium, $\text{cm}^2 \text{sec}^{-1}$,

D_{eff} = effective diffusion coefficient of water in helium within the porous graphite structure, $\text{cm}^2 \text{sec}^{-1}$, D_1 and D_2 being the particular values within the sheath and fuel material,

$\alpha = \frac{Q \Delta T}{2R T_o^2}$, and describes the increase in reaction rate due to temperature gradient,

Q = mean activation energy of oxidation rate kcal. mole^{-1} ,

R = Gas constant,

T = Temperature at point in question, $^{\circ}\text{K}$,

T_o = Temperature at outer surface, $^{\circ}\text{K}$,

$y = y_o \exp(\xi \alpha)$,

$y_o = \frac{Q_o}{\alpha}$,

A and B are functions independent of y but dependant of distance up the channel : A_1 , A_2 and B_1 , B_2 are the corresponding specific values for the sheath and fuel zones respectively,

$K_o(y)$ and $I_o(y)$ are modified Bessel functions of order zero with K_1 and I_1 being the corresponding first order functions,

y_1 and y_2 are the boundary items of y at the sheath inner face and fuel centre line respectively,

P = total gas pressure, atm.,

k_T = chemical reactivity of the graphite at T $^{\circ}\text{K}$, $\text{mg cm}^{-2} \text{h}^{-1}$, $\mu\text{atm}^{-1} \text{H}_2\text{O}$,

B_L = local burn off at any particular position.

These were solved using a Gaussian elimination procedure to calculate the variation of corrosion rate with depth beneath the surface of the pin sheath and the fuel compact material.

Figures 10-13 show the corrosion profiles calculated for a series of channel positions in an early HTR design and show clearly the transition from two zone to in-pore diffusion corrosion as temperatures rise along the channel.

With regard to reactor application, calculation of this two zone case is dependant on the allowed water content and hence on the corrosion assessment of the overall core. Figures 10-13 are thus only indicative of behaviour. There is also a need for improved data on the parameters relevant to corrosion for sheath and fuel material.

This method of analysis is only valid where there is no significant change in relevant graphite properties as a result of corrosion: for the present two zone cases computed so far, the extent of local corrosion is low enough to avoid significant change in properties.

Table 14. Experimental Results on Compact Manufacturing

Characterist. Specificat.		Aver. obtain.	Spread.	Control	
				Accuracy	Method
U235 content	$N \pm 4\%$	$N \pm 2\%$	$-2\% + 0\%$	$\pm 1\%$	γ counting
HML	1.0 ± 0.05	0.97	$0.96 - 0.98$	± 0.02	calc.
Graph.dens.	1.75 ± 0.05	1.70	$1.64 - 1.77$	± 0.1	immersion
Weight	$N \pm 2.5\%$	$N \pm 3\%$	$-4\% + 2\%$	$\pm 0.1\%$	weigh.
Outer $\emptyset$	$N \pm 0.05$	$N \pm 0.04$	$-0.03 + 0.01$	± 0.05	micro.
Inner $\emptyset$	$N \pm 0.05$	$N \pm 0.02$	$-0.01 + 0.01$	± 0.05	micro.
Height (mm)	$N \pm 0.5$	$N \pm 1.5$	$-1.5 + 1.5$	± 0.05	micro.
Contam.fVC	$< 10^{-5}$	10^{-7}	$10^{-5} - 10^{-8}$	unknown	α counting
Fraction broken part.	2×10^{-4}	$< 10^{-4}$	$0 - 3 \times 10^{-4}$	unknown	desintegr.

N indicates the nominal value for each characteristic.

Corrosion Calculation
where Local Property Changes occur

The above mathematical model does not allow for a variation in graphite structure arising from corrosion. However at the high local burn offs near the external surface of high temperature fuel sleeves, the porosity of the graphite structure will be opened up allowing easier in-diffusion of water.

In the analysis above, the term ϕ_o includes a factor D_{eff} which is the effective diffusion of water in the porous medium.

This can also be written :

$$D_{eff} = D_o \lambda \left(\frac{T}{273.2} \right)^{1.75} \left(\frac{1}{p} \right) \quad (10)$$

where

$$\lambda = \frac{\text{diffusion coefficient of H}_2\text{O in He in porous medium of graphite}}{\text{diffusion coefficient of H}_2\text{O in He in free gas conditions}}$$

Using this and the treatment of Wicke it can be shown that :

$$\gamma = \exp \left\{ -d \sqrt{\frac{3.487 \times 10^4 k_T P}{D_o \lambda T^{0.75}}} \right\} \quad (11)$$

Of these only k_T and λ could change as a result of local corrosion. There is evidence from Dragon work that large changes in λ arise from corrosion: whilst k_T must be expected to change the extent is unknown and in this analysis changes have been ascribed to λ only (but could alternatively be considered as a change on the ratio of $\frac{k_T}{\lambda}$ for the corroded state compared to the uncorroded).

From equation 11 and for isothermal conditions each profile of log (corrodant concentration) plotted against depth is a straight line of characteristic slope for the particular λ value.

The local burn off at the surface is dependant only on the chemical reactivity of the graphite and at constant temperature and coolant water content increases linearly with time, ultimately reaching 1000 mg/g^{-1} . When that stage is reached there then exists beneath this surface a corrosion profile which is a composite of all the cumulative corrosions at each point allowing for the changes in graphite properties that have occurred progressively up to the point burn up reached. Further corrosion will not change this profile but will move the whole profile bodily inwards.

To compute this progressive build up of corrosion allowing for the change of graphite properties, a programme has been written which evaluates the profile of corrodant concentration for conveniently small step increases of surface local burn off, say 10 mg/g (and hence a specific time interval), taking account of the selected λ to B_L correlation.

Thus the initial steps will have the slope characteristic of the λ of the virgin graphite. However when the local burn off of layers beneath the surface reaches that at which a change in λ occurs then the slope $\frac{\delta y}{\delta d}$ for the affected layer will be decreased to that characteristic of λ for that burn off: hence the corrosion occurring in this affected layer in the specific time interval can be calculated.

Thus for each successive 10 mg/g increase in surface burn off, the corrodant concentration profile is computed taking the λ value ascribed to the burn off that the layer had reached at the start of this particular period and hence the progressive corrosion in depth can be calculated and the total corrosion and corrosion rate evaluated for each 10 mg/g step. Thus the cumulative corrosion characteristics are evaluated until the equilibrium profile is reached.

There is insufficient published evidence to establish an adequate λ to B_L correlation. However some Dragon work did show experimentally determined (local corrosion) /depth profiles for specimens corroded at 1 atm. He pressure. Therefore various λ / B_L correlations were arbitrarily taken and used to compute the expected corrosion profile for the experimental conditions until a reasonable similarity of corrosion profile and corrosion kinetics was obtained. This work indicated that the λ / B_L correlation was:

- i) far from linear being more sigmoidal in form,
- ii) appeared to tail out to a λ value well below unity.

Considerably more data is needed to allow better evaluation of this λ / B_L correlation and also to determine whether k_T varies substantially with B_L .

Since all the known experimental determinations of graphite corrosion kinetics have corroded the specimens in low pressure gas and since the mathematical model shows that rigorous prediction of the high pressure corrosion kinetics must take account of the variation of graphite properties with burn off, the model is considered to be of considerable value for reactor corrosion predictions and ultimately to have scope for development for reactor operation core corrosion management.

It is to be noted that this treatment is not absolutely rigorous in that there is a discontinuity in $\frac{dy}{dy}$ whereas in the treatment described on page 33 the method used avoids this discontinuity because the properties of the graphite are considered constant. However comparison runs against a rigorous routine have shown that the difference is insignificant.

IRRADIATION BEHAVIOUR

Our program is entirely focused towards the demonstration of the behaviour of HTR fuel; it is not broad enough to incorporate research aspects, for which we rely entirely on the Dragon Project.

The majority of our irradiations are therefore standard endurance tests performed in the BR 2 and BR 3 (Mo1) and the DRAGON reactor; most of them are still in progress or have been reported elsewhere. Only two examples where new results are available will be reported here.

Transient Tests

This series of tests investigates the response of the fuel to power surges, resulting in either a sudden increase of the rating, either a high temperature, either a steep temperature gradient, or more than one of these phenomena at a time. The program started with fresh fuel and extreme conditions:

- power increased in 30 min.,
- at power during 2 hrs,
- particle rating: 0.08 - 0.11 W/particle and
1.3 - 1.9 W/particle,
- average gradient up to 400° C/mm (at 2200° C),
- max. temperature over 2700° C.

As expected, the particles did not survive the extreme gradient and temperature conditions. The set of results fit fairly well the design correlation proposed by the Dragon Project ⁶.

Evolution of Heat Transfer Characteristics

The boiling water capsule, utilized since many years for our plutonium program ¹⁰ has been modified for the irradiation of coated particles.

The internal cavity usually filled with the sodium-potassium alloy and the fuel rod is occupied by the sample with the gap between the sample and the heat diffuser filled with helium.

The prototype capsule is presently being irradiated and contains specimens of fuel 15 mm diameter comprising a graphite can and a fuel stick of coated particles. These fuel sticks comprise holes in which thermocouples are inserted, so that at all times and in various sections along the fuel length, the heat rating, some surface temperature and the central temperature of the sample are recorded. From those data, the heat transfer properties can be assessed; it depends on the location of the thermocouples (the maximum number of thermocouples available is 24) whether it is the overall heat transfer coefficient or the thermal conductivity between two points of the fuel samples which is measured. In our present program only centrally located thermocouples are inserted so that the central temperature of the specimen and the graphite sleeve temperature are known.

Since the irradiation is being pursued, the available results are preliminary in nature and cannot be interpreted until postirradiation examination is performed. As illustration, Fig. 14 shows the evolution with time of the thermal conductivity, calculated from the instrumentation data; it relates to consolidated fuel specimens of the type described on page 31 operating in two ranges of temperature gradients. Fig. 15 compares the rough data obtained under irradiation (CEB) with the results of measurements performed at KFA (Jülich) in a thermal conductivity rig, under very different thermal gradient and heat rating conditions. Fig. 16, incorporating data of the two specimens, seems to indicate a thermal gradient dependence.

Although various interpretations of these results are possible at this stage, a discussion would be premature, the purpose of this presentation being only to illustrate the utilization of an irradiation facility.

CONCLUSIONS

The purpose of our program is to contribute, to the extent of our possibilities, to the development effort of the organizations active in the field of HTR fuel.

A large part of our effort is aimed towards providing statistical and tolerance data to the fuel and core designers.

The reliability of the fuel behaviour is also being investigated and demonstrated. Since the design methods and data of Inter Nuclear Member Companies are utilized to mount the tests and/or to interpret the results, an important fall-out is also obtained in the design of the fuel. A particular example was given for the corrosion problem.

Finally our aim is also to test and demonstrate that appropriate HTR fuel can be manufactured by industrial techniques, on the basis of the experience made available to the signatories by the Dragon Project.

ACKNOWLEDGEMENT

The authors wish to acknowledge the collaboration of many sources and individuals of their organization, for helping in carrying out the program.

A special mention needs to go to the Dragon Project for providing back ground data, contributing with stimulating discussions and cooperating directly to some parts of the program (e.g. irradiations).

The corrosion work described on pages 32 to 43 was carried out for TNPG Limited by D. Birch and P. F. Arthur of International Research and Development Co, Ltd.

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Fig. 1. Kernels manufactured by powder agglomeration.

Fig. 2. Kernel at 90 % TD.

Fig. 3. Kernel at 83 % TD.

FREQUENCY

Fig. 4. Kernel size distribution within a batch.

Fig. 5. Effect of stoichiometry on CO pressure.

Fig. 6. Standard coating layers.

Fig. 7. Pressed compact.

Fig. 8. Components of a fuel rod.

Fig. 9. Influence of the raw material on the texture of the matrix.

Fig. 10. Corrosion profile across annuli at the channel inlet at peak random temperature.

Fig. 11. Corrosion profile at the 37.5 cm level at peak random temperature.

Fig. 12. Corrosion profile at the 100 cm level at peak random temperature.

Fig. 13. Corrosion profile at the 137.5 cm level at peak random temperature.

Fig. 14. Evolution of the thermal conductivity of consolidated fuel (irradiation temperature 500-700°C).

Fig. 15. Influence of the average temperature on the thermal conductivity of consolidated fuel.

Fig. 16. Influence of the thermal gradient on the conductivity of consolidated fuel (irradiation temperature 500-700°C).

DISCUSSION

K. Hackstein: You showed a range of coating thicknesses of 10 microns in your statistical tests. Could you indicate on what size of coating batches this was obtained?

H. Bairiot: The statistical data we reported were obtained from fabrication of fuel for a criticality test. The limited quantity of kernels available, the necessity for insuring a minimum quantity of waste and the desire to gather significant statistical data led us to select one kg HM as a coating batch size. Undoubtedly the range of coating thicknesses would be larger for larger coating batches.

HTR FUEL AND MATERIALS DEVELOPMENT IN GERMANY.
PRESENT SITUATION AND PROGRAM

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ABSTRACT

The German HTR program includes fuel and graphite development and irradiation testing. The work is carried out jointly by the German industry and the research center KFA Jülich. Pressed spherical fuel elements have been produced and loaded into the AVR since 1969 and will be used for THTR. The experience obtained with the AVR and the results of irradiation tests indicate that these elements will perform well in THTR and are well suited for larger power reactors. Parallel work on prismatic fuel elements has been started and will be increased in the coming years. Design data on the irradiation induced changes of the physical properties of commercially available and newly developed graphites for fuel elements and reflectors is provided by irradiation tests up to life-time doses. In addition a research and development program for new types of graphite for HTRs has been started.

INTRODUCTION

The German high temperature reactor development is backed up by increasing efforts in fuel and materials development. These are concentrated in a cooperative German Fuel and Materials

Program for HTRs which serves for the development of fuel elements and graphites with special consideration for the following reactors and projects:

AVR 15 MWe in operation

THTR 300 MWe beginning of construction in 1970

HHT 600 MWe design study to be completed in 1973

The work on fuel and fuel elements is carried out by the companies Brown Boveri/Krupp (BBK), Gutehoffnungshütte (GHH), NUKEM and the Kernforschungsanlage Jülich (KFA). The reactor building companies BBK and GHH are mainly responsible for fuel element design and specification development. NUKEM fabricates the fuel and develops fuel element materials and production processes. Graphites for High Temperature Reactors are developed by Sigri Elektrographit and Ringsdorff-Werke. KFA is responsible for all irradiation tests including post-irradiation examination and contributes to materials research and development. The work is sponsored by the German government and supported by close cooperation with the Dragon Project. The activities can be subdivided into work on coated particles, fuel elements and graphites. Each of these sections includes irradiation testing.

COATED PARTICLES

Fuel Kernel Preparation

The $(U,Th)C_2$ fuel particles of approximately 0.32 mm diameter presently used in the AVR fuel elements are still produced by an agglomeration technique and subsequent melting in a bed of graphite powder. It is envisaged that we will change to oxide

particles in 1971. These will then be used for follow-up charges of the AVR as well as for the THTR and HHT. Production equipment for oxide particles has been set up and is used for the fabrication of particles for irradiation tests. The particles are produced from an emulsion by a surface tension method and sintering. In addition sol-gel-processes for UO_2 and $(\text{UTh})\text{O}_2$ particles of diameters up to 1 mm have been developed on a laboratory scale.

Coating Deposition and Characterization

The fuel particles for AVR and THTR have pyrocarbon coatings only, while direct cycle helium turbine plants as studied in the HHT project probably will require a silicon carbide interlayer. Fluidized beds with a capacity up to 3 and 5 kg per batch have been developed for production coating. Studies on the influence of deposition parameters on coating properties are presently concentrated on SiC and on coatings obtained from propylene.

Emphasis is given to the characterization of coating structures and properties before and after irradiation. Two new methods have been added to those existing: and optical method called Optaf for determining the anisotropy of pyrocarbon and a method for measuring the modulus of elasticity and the rupture strength of outer coating layers. Optical determinations of the anisotropy are the only methods which can be applied directly to the coated particles. The modulus of elasticity and the rupture strength are calculated from values obtained by pressing the coated particles between two parallel plates and measuring deformation and force.

Model Work and Irradiation Testing

Specification tests and tests for the comparison of the results of model calculations with actual coated particle performance have been carried out up to doses of 10^{22} n/cm² ($E > 0,1$ MeV) and will be continued on an increasing scale. The influence of time, temperature and burn-up especially with respect to solid fission product diffusion is studied in the Dido reactor of KFA which has low fast flux. The effects of high fast neutron doses are investigated by tests in the DFR Dounreay (Paper 8/133). Performance tests and accelerated performance tests combining both the effects of fast neutron dose and burn-up are carried out in the Dragon and in MTRs (R-2, BR-2). Particle types which will be used for the production of fuel elements are subjected to final specification tests in their operating environment. This means that they are usually tested in the matrix material which will accommodate them in the fuel elements.

FUEL ELEMENTS

For the pebble bed reactors AVR and THTR, spherical fuel elements of 6 cm diameter will be used. Whether spherical or prismatic fuel elements will be utilized in the HHT will be decided at a later stage.

Spherical Fuel Elements for Pebble Bed Reactors

Most of the development work on fuel elements has been performed for the spherical fuel elements of AVR and THTR. At present the AVR operates with the three types of fuel elements shown in Fig. 1. The

first fuel elements which were fabricated by Union Carbide Corp. contained the coated particles in an injection moulded and heat treated matrix inside a machined graphite shell. The second type which has been developed by NUKEM has only been fabricated for a short time. These elements also have a machined graphite shell to which the particles were attached by bonding. The inside of the element is filled with graphite flour which is compacted to a density above 1.7 g/cm^3 . An extensive development program, which started under the THTR-association (BBK, EURATOM, KFA) enabled NUKEM to develop fabrication processes for a more advanced and economical third type of elements. These are fabricated by pressing and heat treating a mixture of graphite powders, binder materials and coated particles (Paper 9/116). Fuel elements of this type have been loaded into the AVR since 1969 and will be used for THTR and are considered for HHT.

Fig. 2. shows a crosssection of one of the UCC fuel elements irradiated in AVR for 8-10 % fima and a fast dose of $1.2 \times 10^{21} \text{ n/cm}^2$ ($E > 0,1 \text{ MeV}$). Spearhead attack has started, but did not lead to an increased activity in the reactor. Fig. 3. shows particles in a pressed sphere after irradiation in the test SK 1 to 10 % fima and a fast dose of $2.2 \times 10^{21} \text{ n/cm}^2$ ($E > 0,1 \text{ MeV}$). The excellent performance of the two balls irradiated in this test was also demonstrated by the small fission gas release of $R/B = 2 \times 10^{-5}$ for ^{133}Xe , which was well below the specified value of $R/B = 5 \times 10^{-4}$.

Fig. 4. gives a survey on the irradiation of spherical fuel elements in various tests and in the AVR. The numbers in the circles indicate the numbers of fuel elements irradiated in the tests. Circles with an arrow indicate the present burn-up and

dose of tests which are still in progress. The guaranteed values for the fuel elements of THTR have been reached by the tests SK 1 and SK 2. SK 2 will reach more than 14 % fima and a fast dose of $6.4 \times 10^{21} \text{ n/cm}^2$ ($E > 0,1 \text{ MeV}$) during 1970. SK 3 will reach these values by the middle of 1971. In the THTR less than 10^{-4} of the fuel elements leaving the reactor will have reached these values which are 20 % above average. Future tests of the SK series will include irradiation of feed elements to 70 % fima and a fast neutron dose of 4×10^{21} and of breed elements to 14 % fima and a fast dose of $8 \times 10^{21} \text{ n/cm}^2$.

In one experiment at Jülich spherical elements were tested 37 days at a rating of 8 kW per element (instead of the maximum design value of 3.3 kW) and with surface temperatures of 1400°C (instead of 1000°C). No damage was observed.

Fig. 5. shows how temperature and rating of a fuel element during its runs through a pebble bed reactor are simulated by the SK tests. The figure demonstrates that the fuel temperature in the test is always above the fuel temperature in the reference reactor. This creates more stringent conditions than the reference case and thus provides for sufficient safety of the demonstration.

Tests of the graphite matrix of the pressed elements in the DFR have demonstrated that no detrimental changes occur at the maximum life-time dose of $6.4 \times 10^{21} \text{ n/cm}^2$.

The experience with the fuel elements of the AVR and the available results of irradiation tests indicate that the pressed fuel elements are well suited for THTR and larger power reactors.

Prismatic Fuel Elements

Despite of the encouraging results obtained with the spherical fuel elements parallel work on prismatic fuel elements has been started and will be increased in the future . The development of matrix materials and fabrication techniques for fuel compacts is based on experiences from the pressed spherical fuel elements and on Dragon know-how. The influence of composition, temperature of pyrolysis and other parameters on the properties of materials for compacts and to a smaller extend for bonded beds are being systematically investigated. Annular compacts and smaller samples for materials development are being irradiated to various doses up to $6 \times 10^{21} \text{ n/cm}^2$ ($E > 0,1 \text{ MeV}$) in Dragon, DFR Dounreay and MTRs.

GRAPHITE

Design data on the irradiation induced changes of the physical properties of commercial types of graphite for prismatic fuel elements and reflectors is obtained from irradiation tests in DFR Dounreay, Dragon and MTRs. A Gilso carbon graphite will used for the inner reflector of the THTR. Its maximum life dose will be $1.2 \times 10^{22} \text{ n/cm}^2$ (Dido Ni) at temperatures between 300 and 1000 °C. This dose has been surpassed in UKAEA tests with AGL graphites and will be reached in our tests with Sigri graphites in May 1971.

In additional programs new graphites for HTRs are being developed and the influence of composition and fabrication parameters on the irradiation behavior is being tested. Studies on the

feasibility of replacing Gilso carbon by various other raw materials and on special forming methods like isostatic pressing and vibratory compaction are included. The reproducibility of promising types is being checked by production runs.

Fig.1. Types and Amounts of Fuel Elements Presently Used in the AVR

Fig.2. Particle of an UCC-Fuel
Element Irradiated in AVR
8-10 % fima, $1.2 \times 10^{21} \text{ n/cm}^2$ ($E > 0.1 \text{ MeV}$)

Fig.3. Particle of a Pressed Fuel
Element Irradiated in SK 1 Test
10.5 % fima, 2×10^{21} n/cm² ($E > 0.1$ MeV)

Fig. 4. Irradiation Tests of Spherical Fuel Elements

Fig. 5. Fuel Temperature in the THTR and in Fuel Demonstration Tests SK 2 and SK 3

DISCUSSION

J. H. Coobs: Could you comment on the relative merits of spherical and prismatic fuel elements for HTGR's and on the present status of development and testing?

B. G. Liebmann: Thank you for this question, which gives me a chance to make some propaganda for the spheres. As far as I can see they have two advantages: 1. There is production and irradiation experience with spherical fuel elements. Spherical fuel elements are being produced for the AVR and have been irradiated up to doses and burnups as guaranteed for the finished core of THTR. For irradiation behavior we do not have to rely on tests of small fragments of fuel elements which is the case for prismatic elements. We have tested the complete elements. 2. Pebble bed reactors have the advantage that only a few fuel elements see the highest temperatures and this only for a few days of their lifetime. In reactors with block type elements the same elements remain under the most severe conditions for a long time. Also the worst fuel temperatures seem to be lower in pebble bed reactors than in reactors with prismatic fuel elements and the same outlet gas temperature. There are, of course, also disadvantages to pebble bed reactors, but these are not concerning the fuel elements themselves. As I have said in the paper, we are now also engaged in the development of prismatic fuel elements. One reason for this is our belief that international collaboration is required to bring HTR's to a success. Therefore, we want to be prepared to produce prismatic fuel elements also.

BEHAVIOR OF COATED PARTICLE FUEL IN
DFR IRRADIATION EXPERIMENTS

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ABSTRACT

The fast - neutron damage of several types of pyrocarbon coated particle layers was investigated by two irradiation experiments in the Dounreay Fast Reactor (DFR). The maximum fast dose accumulated in both experiments was $5.5 \cdot 10^{21}$ n/cm² and $14.6 \cdot 10^{21}$ n/cm² ($E > 0,18$ MeV) respectively, the temperature 1300°C.

The results indicate that density and Bacon anisotropy factor (BAF) of pyrocarbon layers determine the irradiation behavior. We found, however, that a density of about 1.90 g/cm³ and a BAF of about 1.03 alone do not guarantee the particles to remain intact after a dose of $14.6 \cdot 10^{21}$ n/cm². It is assumed another property which was not measured in the experiments to be important at such high fast flux irradiations.

INTRODUCTION

Stresses in the layers of coated particles are mainly due to the following effects:

- build up of gas pressure between kernel and coating
- anisotropic thermal dimensional changes
- interactions between kernel and coating and between the coating layers
- fast neutron induced dimensional changes of pyrocarbon.

In order to study the influence of the fast neutron flux on coated particle behavior separately, two irradiation experiments in the Dounreay Fast Reactor (DFR) were carried out by the Thorium-Hochtemperatur-Reaktor - (THTR) Association in the last three years.

IRRADIATION DATA

The two Dounreay irradiation experiments differ mainly in the irradiation time and the accumulated fast dose. Table 1 shows some details of the irradiation data.

Table 1: Irradiation data of the Dounreay experiments DN 1 and DN 2

Experiment	DN 1	DN 2
Full power days	50	96
Maximum fast dose ($E > 0,18$ MeV)	$5,5 \cdot 10^{21}$	$14,6 \cdot 10^{21}$
Temperature	1300°C	1300°C
Burn up	<1 % fima	<1 % fima

DESCRIPTION OF TEST SPECIMENS

In both tests a variety of different particle batches was tested. Most of the particles were of the BISO type, i.e. the UC_2 - kernel was coated by a porous buffer layer and a dense pyrocarbon outer layer. Fig. 1 shows a metallographic section of such a particle with dimensions used mostly in this test. The porous buffer layer provides volume for the fission gases and decouples the outer layer from the kernel mechanically. The outer layer is the pressure vessel for the fission gases and the diffusion barrier for the solid fission products.

The BISO type particles in these experiments can be divided into several different series, in each of which one parameter is systematically varied. These parameters are kernel composition, layer thickness and properties of the outer layer pyrocarbon.

From each batch 40 spherical particles with equal layer thicknesses were selected for irradiation by means of radiographs. Thus it was certain that the results of the irradiation were clear-cut: All particles of a batch failed or all particles remained intact.

FAST NEUTRON INDUCED DIMENSIONAL CHANGES

Since the fission rate is very small in the Dounreay test, the outer layer is put into stress only by the fast neutron induced dimensional changes of the pyrocarbon. Therefore these tests can be considered more as a special material test than as a coated particle test.

Fast neutron induced dimensional changes of free pyrocarbon specimens were measured for instance by GGA¹. Fig. 2 shows these changes for different types of pyrocarbon. The curves are extrapolated up to the maximum dose reached in DN 2. They show that at the maximum dose, for instance, a $1,9 \text{ g/cm}^3$ pyrocarbon would swell in radial direction by 330 % while it would shrink in circumferential direction by about 42 %. These dimensional changes are even higher at higher densities or BAF - values. In pyrocarbon layers of a coated particle those changes must be balanced by stresses and creep.

To analyze the stresses in the layers of coated particles models have been developed, for instance, by Prados and Scott (ORNL)², Kaae (GGA)³ and Walther (SORIN)⁴. These models take into account all the effects presented here at the beginning, but they can also be used for calculations of the stresses induced by dimensional changes only. Calculations with the models of Prados, Scott and Walther have been made and compared with the results of the experiments described here. Our results were found to be in a good agreement with the calculations.

RESULTS

Influence of the outer layer thickness on particle behavior

In one series of four batches the outer layer thickness was varied from 30 μm to 150 μm . After irradiation in DN 2 the particles of the four batches were examined under the periscope and all of them were found to be intact. In the metallographic section, however, radial cracks were found in the 115 and 150 μm thick layers which arose during sectioning. (Fig. 3). These cracks indicate that there were high tensile stresses in the layers nearly as high as the rupture stress. Another effect is

seen in Fig. 3: The thicknesses and the porosity of the buffer layer of the two particle batches differ after irradiation. The buffer of the particle with the thicker outer layer seems to be thinner and less porous than the buffer of the other particle which had a thinner outer layer. This indicates that much more pressure is put on the buffer by the shrinkage of the thicker layer. In fact, model calculations confirm these observations.

Influence of the pyrocarbon properties

The influence of pyrocarbon properties on the irradiation behavior of the particles has been found more important than all the other parameters studied in both the DN 1 and DN 2 experiments.

The following pyrocarbon properties were investigated:

1. Density: measured both on pyrocarbon disks and on coating fragments by the sink-float-method,
2. Orientation anisotropy, specified as Bacon-anisotropy factor (BAF) measured by an x-ray diffraction technique on disks and by an optical method on metallographic sections of particles,
3. Apparent crystallite size: measured by an x-ray diffraction technique on disks.

It was not possible, however, to vary all these parameters independently in methane deposited layers and this complicates the interpretation of the results.

The post irradiation examinations show that particle layers remained intact only when density and BAF are below a certain limit. These limits differ for the two experiments, they are shown in Table 2. An influence of the crystallite size could not be found.

Table 2: Properties of pyrocarbon layers which remained intact in DFR experiments

Experiment	DN 1	DN 2
BAF	< 1,2	< 1,05
Density [g/cm ³]	1.6 - 2.05	1.6 - 1.95

Damage mechanism of the pyrocarbon layers

The following damage mechanism of the particle layers has been observed: At one point of the outer surface the layer cracks towards the inner surface and many cracks start from here and spread in radial directions. The particle looks like an opening flower. After cracking, the stresses in the pyrocarbon are much lower than before and therefore the pyrocarbon can change its dimensions as shown in Fig. 2. The Figures 4 and 5 show this history schematically on two particle batches which seem to have been destroyed at different times in the irradiation history

Dimensional changes and anisotropy changes of intact particles

In the DN 2 experiment three particles were taken from nearly each batch and were irradiated as individual particles. The dimensions of these particles have been measured precisely by radiography before and after irradiation. In all cases a decrease of the outer diameter was found. The porous inner layer densified about 30 % and we found a certain decrease in the UC_2 kernel diameter too, while the diameters of the $(U,Th)O_2$ kernels did not change. Some details of two particle batches are given in Table 3.

Anisotropy has been measured on metallographic sections before and after irradiation. As expected, the BAF values increased strongly; for instance, from $BAF = 1.00$ to $BAF = 1.80$ after a fast dose of $14.6 \cdot 10^{21}$ n/cm².

Changes of dimensions and density of broken layers

Pyrocarbon thicknesses of broken layers were measured and compared with the original thickness. The comparison shows a swelling of pyrocarbon with an original density of 2.1 g/cm^3 and an BAF value of 2.0 up to a factor 5 to 6. The swelling of pyrocarbon with lower original densities and BAF - values was lower. These swell factors were found to be in a good agreement with extrapolated dimensional change curves of GGA¹ (Fig. 2).

Density measurements were made on pyrocarbon fragments of all the batches which failed in the experiment DN 2.

Original densities of the different batches lay between 1,6 and 2,1 g/cm³ and after irradiation to $14,6 \cdot 10^{21}$ n/cm² densities of all charges were in the range of 1.8 to 1.9 g/cm³.

Discussion of the results

As shown before density and BAF value determine the fast neutron irradiation behavior of pyrocarbon layers on coated particles. But we found in the DN 2 experiment that these conditions are necessary but not sufficient. Some batches of particles failed at the same irradiation conditions although their density and anisotropy fulfilled the conditions mentioned above. However, the conditions used in depositing the outer layers, especially the deposition rate, differ for the batches which remained intact and which failed.

In this manner, we came to the conclusion, that there are one or more other pyrocarbon properties which were not investigated in the present experiments but which are obviously of great influence under the extreme irradiation conditions of the Dounreay experiments. Therefore some effort is now being made on measurements of mechanical properties of particle layers (Young's modulus) and of the non-crystalline contents within the pyrocarbon layers.

CONCLUSIONS

The irradiation experiment DN 2 shows that coated particle pyrocarbon layers with certain material properties deposited from methane can withstand extreme high fast neutron fluxes up to double the dose reached in high temperature gas-cooled reactors at the maximum. Those properties are: density 1.6 to 1.95 g/cm³, BAF < 1,05 with the additional condition that the deposition rate has to be greater than 100 μm/h.

Results of the DN 1 experiment indicate that these values are less critical up to the maximum fast dose of an HTGR. (density: 1.6 to 2.00 g/cm³, BAF < 1.2)

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Table 3: Dimensional changes of intact coated particle batches after $14.6 \cdot 10^{21}$ n/cm² (E>0,18 MeV) at 1300°C, burn up < 1 % fima.

Batch	Unirradiated			Irradiated		
	Kernel	Buffer	Outer Layer	Kernel	Changes of	Outer Layer
	Diameter /um	Thickness /um	Thickness /um	Diameter %	Thickness %	Thickness %
MAUC 51	375	43	65	- 3	- 28	+ 15
MAUC 53	390	40	150	- 4	- 36	+ 13

Fig. 1. BISO - Coated Particle

Fig. 2. Bulk Dimensional Change Versus Fast Neutron Exposure for Different Pyrocarbons, Irradiated at 1250°C. (after GGA¹)

Fig. 3. Influence of Outer Layer Thickness on Coated Particle Behavior in High Fast Neutron Flux.

Fig. 4. Damage of Coated Particle Pyrocarbon Layers by Irradiation Induced Dimensional Changes. (schematic Drawing).

Fig. 5. Damage of Coated Particle Pyrocarbon Layers by Irradiation Induced Dimensional Changes.

DISCUSSION

D. M. Hewette: We have been doing parallel coated-particle tests in the HFIR target region obtaining results at slightly lower temperatures (1100°C) than yours but at fluences near your values. We have tested high density isotropic propylene coatings densities as high as 2.07 grams per CMQ and find that these coatings survive. Tests at GGA suggest that propylene coatings don't increase in anisotropy nearly as much as do methane. Do you plan to test propylene derived coatings in the future?

J. Baier: Yes, we have such particles in the HFIR HT-5 test and are awaiting evaluation. However, we can't compare these coatings directly with methane because of low crystallite sizes.

D. M. Hewette: The crystallite size of the propylene coatings can be increased by heat treatment to give values the same as for high temperature methane coatings.

J. Baier: We didn't increase the crystallite size by heat treatment in our first experiment. But in our further experiments we will do so.

J. L. Scott: What is the mechanism of change in preferred orientation of pyrolytic carbon under irradiation?

J. Baier: I cannot fully answer this question at the moment. It can be assumed, however, that the increase in preferred orientation is due to creep.

D. Tytgat: I want to complement Dr. Graham's and Dr. Liebmann's comments on the irradiation in fast reactors. Dr. L. Valette from Euratom proposed last year at the AIEA, June 1969, meeting in Vienna that for graphite irradiations, a good correlation exists between the different dimensional changes measured in different reactors for temperatures as high as 1200°C, if the appropriate corrected temperatures are calculated by using the same activation energy $Q = 1.2$ ev as Dr. Simmons from Harwell. This value was proposed initially for temperatures up to about 500°C. It would be interesting to see if the same effect is observed for pyrocarbon irradiations performed by different laboratories. Could Dr. Baier comment on how his data fitted with the others?

J. Baier: We compared our dimensional measurements both on broken layers and on free pyrocarbon specimens with the extrapolated curve of GGA and found a rather good agreement. Our values tend to be a bit lower than those of the extrapolated curve.

K. Wirtz: In one of your experiments particles with higher coating densities cracked to a higher degree than those with longer ones. Do you think that with higher temperatures and longer times for the irradiation to the same dose, i.e., irradiation in a lower flux would give better results? More generally, what is the influence of temperature and flux density on your result? Is their influence of internal creep?

J. Baier: The question concerning the influence of higher fast neutron flux is not yet answered by experiment. Therefore, we are just planning experiments, for instance, in the HFR-Petten up to same fast fluences as in the DFR experiments. In the work of GGA (Bokrus) on free pyrocarbon specimens a strong influence of the temperature on the dimensional changes and on creep behavior was found. In our experiments we could not investigate the influence of temperature because the fast fluence was lower at lower temperature.

L. W. Graham: I would like to make a comment on the relevance of irradiations under the high-accelerated fast flux conditions in DFR. A great deal of work has been published relating to low temperature irradiation in DFR on graphites developed for the AGR. Much of the concern in this work has been with correction factors which have to be applied to the temperatures to give an "equivalent irradiation temperature"; equivalent that is, to a temperature in a power reactor possessing a much lower fast flux. From experiments done on graphite in both DFR and on graphite test rigs in the HFR-Petten where the irradiation temperatures were the same and higher we find quite different dimensional changes indicating that large equivalent temperature corrections have to be made for high temperature graphite irradiation in DFR. The correction is a downward one and can be several hundred degrees centigrade. It is difficult to justify such corrections theoretically by the methods used previously for the low temperature AGR work. I personally believe that this is a consequence of the temperature dependence of irradiation creep above 500°C.

A downward equivalent temperature correction describes the correction for the displacement in the crystals, but the creep occurs equivalent to the actual irradiation temperature. All this leads me to believe that irradiation of graphite materials at high temperatures in the DFR are only of qualitative use.

EXPERIENCE WITH THE PRODUCTION OF
AVR-FUEL ELEMENTS

K. G. Hackstein

6159000

ABSTRACT

More than 20,000 of the so called molded fuel elements were made for the AVR-Reactor. A critical examination of the most important data obtained during fabrication of kernels coated particles and spheres indicates that the production of large amounts of this fuel elements can be achieved without serious difficulties. Further development work has to be done with respect to the reproducibility of some properties.

Introduction

In addition to activities in the development of production methods for kernels, coated particles, prismatic fuel elements including blocks, we are now producing fuel elements for the AVR-Reactor.

The AVR-Reactor needs about 18,000 fuel elements per year. Since May 1969 the reactor has been reloaded with the so called molded fuel elements developed by Nukem.

We now have made more than 20,000 fuel elements of this type and will give a short review of the experience and results during this production.

Fuel Element Design

The spheres contain 6 g heavy metal in the form of Uranium/Thoriumcarbide (ratio is 1:5) kernels coated with a duplex layer of pyrolytic carbon. The thickness of the inner layer is in the order of 50 microns and the density is about 1 g/cm^3 . The thickness of the outer layer is 120 microns and the density was designed to be $\geq 2,00 \text{ g/cm}^3$ for the first

5 batches, but was then reduced to $2,00 \pm 0,1$ g/cm³ (one homogenized batch was 30 - 40 kg coated particles).

Based on irradiation results it may be necessary later on to reduce the density even more, but for the burnup conditions this is not now a very important question.

Carbide coated particles are used, because they have been sufficiently tested as loose particles as well as embedded in spheres. We will employ oxide coated particles as soon as the irradiation tests for full burnup (more than 12 % fima) and the desired fast dose ($7 \cdot 10^{21}$ nvt) are finished. These tests are under way at Studsvik the fast dose is now about 3,5 to $4 \cdot 10^{21}$ nvt (E 0,1 MeV) and the burnup is 12 - 13 % fima.

Coated Particles

So far we have produced about 600 kg coated particles of the carbide type which have been used for both the earlier wale paper and molded spheres, but mainly for the latter. During production the following properties have been measured on the dense melted carbide kernels.

Uranium/Thorium ratio
Carbon content
Density
Sphericity

On coated particles we have measured

Thickness of the buffer layer
Thickness of the outer layer
Density of the outer layer
Anisotropyfactor of the outer layer
Surface contamination.

Table 1 represents some data of kernels and coated particles obtained from 17 batches.

This measurements were made on batches of about 20 kg heavy metal resp. 40 kg coated particles. In addition microradiographic and metallographic measurements were made in order to detect Uranmigration, cracks etc.

Parallel to this the reproducibility of the coating conditions was controlled by measuring the thickness of the inner and outer layer of 18 single coating batches. One batch was 1 kg heavy metal.

Table 2 indicates that the values and the corresponding standard deviations are relatively constant.

Fuel Elements

The pressed spheres contain 76 % natural graphite, 14 % graphitized petrol coke and 10 % binder coke. During production we have learned that the properties of the fuel elements are depending mainly on the quality of the raw materials. Therefore the following analyses on graphite powder are made

Structure (Metallography and X-ray)

Density

Electric resistance

BET-surface

Grain-size (by sieve analyses and sedimentation)

Buld density

Tap density

Water content

Gas content (H_2 , N_2 , O_2 per hot extraction)

Impurities.

The first parameters, structure density blaine value or BET-surface and electric resistance have a remarkable influence on the properties of the graphite matrix, with respect to the density, thermal conductivity and corrosion rate.

The fracture load and especially the number of drops necessary to cause surface damage of the fuel elements are influenced by the grain-size of the powder.

The binder is also very extensively controlled with respect to the important chemical, physical properties.

Since the fabrication methods for the pressed fuel elements have been described on previous occasions, we will only discuss the more interesting analyses of data obtained during and after fabrication of more than 20,000 spheres.

Table 3 shows the most important data which we think are necessary for a critical examination with respect to quality and reproducibility. Each plot represents one lot, which is 450 fuel elements.

Table 3 also indicates that some of the properties are relatively constant, others are not. As pointed out above the scattering values of the fracture load and the number of drops the spheres withstand without surface damage are corresponding to the variation of the grain-size of the graphite powder.

At present the through-put is about 130 fuel elements per day (one shift). We are now convinced that a through-put of 800 - 1000 or more pressed fuel elements per day as necessary for THTR-Reactors can be achieved without serious difficulties but further development work is still necessary.

DISCUSSION

J. A. Robertson: What confidence is there that optical methods give BAF values representative of bulk properties and not of the surface (which may be affected by metallographic preparation)?

B. Liebmann: There is certainly a problem. The method of surface preparation must be standardized. Comparative measurements with optical and X-ray methods taken on graphite discs have, however, demonstrated the possibility of reliable correlation between OPTAF and BAF.

J. L. Scott: Do you have any data on the shrinkage rate of fuel compacts with lgU/cc compared with unfueled shells which are molded

K. G. Hackstein: No, up to now we don't have the exact figures. We will start with irradiation tests in the middle of 1970.

H. Krämer: Dr. Hackstein should add a word about what heavy metal densities are achievable by pressurizing tubular rods.

K. G. Hackstein: Up to now we have reached a heavy metal density of one gram per cubic centimeter with particles having a kernel diameter of 600 microns. The free uranium is less than 10×10^{-6} .

J. D. Hart: I have understood that you determined particle breakage levels by measuring the diffused uranium after heat treatment rather than by disintegration methods. Do you believe that you could detect one broken particle in 10^4 by this method?

K. G. Hackstein: We can, because the determination of uranium is made after electrolytic disintegrations of the spheres.

R. F. Turner: Were the compacts which showed high fuel density made by the overcoating process?

K. G. Hackstein: We use overcoated particles together with another part of the same graphite powder and press this mixture.

R. A. U. Huddle: In our economic assessment of fuel element production we have concluded that an article made from powder and made by an overcoating process is something of the order of 5-10 times as expensive

as a similar article made from conventional graphite. Would you comment on the part of the element you just described. We do not use POCO graphite.

K. G. Hackstein: We think that is not really a problem if you have a production line running. On the other side, you know that we are using refined graphite powder dust; this is much cheaper from graphitized petrol coke. I didn't mention POCO graphite.

ECONOMICS OF HTGR FUEL RECYCLE
AND POWER GENERATION

(Session VI)

Chairman: H. B. Stewart,
Gulf General Atomic

Co-Chairman: R. S. Carlsmith,
Oak Ridge National Laboratory

THORIUM-FUEL-REPROCESSING RESEARCH AND DEVELOPMENT WORK
AT THE KFA-JUELICH *

J. Bohnenstingl	H. Kirchner	D. Thiele
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453 3000

ABSTRACT

Research and development work at the KFA-Institute for Chemical Technology is directed towards the development of suitable reprocessing methods for the graphite fuel elements of HTG-reactors. This work is part of a joint project in which also several companies of the German chemical industry participate and which is sponsored by the Federal Ministry for Education and Science.

Early in the project, several process concepts were investigated. Finally two basically aqueous process-flow-sheets were established, on which all further efforts are concentrated.

The first-choice process consists of the following steps:

- Burning of the fuel elements.
- Dissolution of the residual oxides either in THOREX-reagent, or in the case of highly refractory oxide particles, in an acid-salt melt.
- Isolation of protactinium by a sorption technique.
- Thorium and uranium recovery and purification by TBP-extraction.
- Waste treatment and solidification.

This process will be demonstrated in the middle of the seventies in the "JUPITER" hot processing pilot plant. The capacity of this plant will be on the order of 2 kg of heavy metal per day. Next to the burning, the dry chlorination of the ground HTGR-fuels could be another advantageous method for separating matrix graphite from the heavy metals. Therefore, the CHLORINEX-flow-sheet developed by KFA, could represent an alternative to the Burn-Leach/TBP-Extraction-Process.

* Work performed under a joint project sponsored by the German Federal Ministry for Education and Science.

In the head-end of this process, the ground material is fed into a fixed bed and chlorinated with elementary chlorine at about 1000°C. The heavy metals are removed out of the chlorinator. The remaining graphite is discarded and stored, or perhaps burned. The condensed chlorides are dissolved and further separated and decontaminated by a solvent extraction cycle. One alternative investigated is the conversion of the chlorides into nitrates and then applying a THOREX-flow-sheet; another possible method is a solvent extraction procedure in the chloride system with long chain tertiary amines diluted with aromatic hydrocarbons.

INTRODUCTION

In the Federal Republic of Germany, so far, the AVR-reactor is the only high-temperature gas-cooled reactor in operation. During the seventies, two more HTGR's with together 900 MW_{e1} are expected to be installed. They will be fueled with coated oxide particles dispersed uniformly in a graphite matrix forming a 60 mm diameter sphere. For the reprocessing of this type of fuel, research and development work especially in the head-end step is needed, due to the fact that a simple mechanical separation of the moderator graphite and fuel material is impossible. In addition, the use of coated particles with their chemically extremely resistant coatings of pyrocarbon and possibly silicon carbide causes additional problems.

In order to guarantee a complete fuel cycle to future reactor users, appropriate procedures for the reprocessing of spent fuel elements must be developed in the near future. Therefore, already in 1966 a joint project between KFA and seven industrial companies was inaugurated by the Federal Ministry for Education and Science.

This paper describes the work done either by the KFA alone, or in close cooperation between KFA and industrial companies. Activities falling exclusively under the responsibility of our industrial partners will not be illustrated here.

SHORT SURVEY OF STUDIES DURING THE EARLY PROJECT PERIOD

During the first three years of the project, a relatively broad spectrum of different reprocessing methods has been investigated on a laboratory scale¹. In that time we have been including the thorium molten-salt reactor in our research program. Also in that time, we followed the overall advocated view that for future use mainly nonaqueous processes should be superior. Therefore we first started with fluorination experiments trying different fluorination agents. The still unsolved main problem is the difficulty of good uranium yields due to the obstinate holdback of the thorium matrix. This problem doesn't exist in molten salt fluorination.

Other activities included pyrometallurgical and pyrochemical methods of molten-salt separation and purification processes applying partition between liquid metal and salt phases. Much effort was put also in such methods like oxidative slugging and electrorefining from salt melts. All methods yielded unsatisfactory separation and decontamination factors as well as severe material and corrosion problems.

We concluded from the results and experience gained during this period that in the first reprocessing period only a wet chemical process such as solvent-extraction can satisfactorily separate the feed and/or breed material from

the fission products. This conclusion is not surprising since in 25 years of experience in reprocessing, only extraction processes have been practical on an industrial scale.

Nevertheless we believe that we got a rather good return out of these studies since they give us a better understanding of the potentialities of aqueous processing and its associated problems. As the result of about four years of work, two process flow-sheets were established finally, on which all further efforts are concentrated.

For the present, an isolation of decontaminated thorium can be renounced. Further, the process should be capable of treating fuel elements after short cooling periods. Therefore, a process step for the recovery and purification of the relatively long-lived ^{233}Pa has to be provided. On the other hand, we are well aware of the fact that the necessary iodine removal may be the other limiting factor in processing short-cooled fuel.

I. BURN-LEACH / TBP-SOLVENT EXTRACTION PROCESS

There seems to exist no doubt today that for a first reprocessing period of HTGR-fuel elements the most expedient flow-sheet will be marked by a burn-leach step in the head-end and a TBP-solvent extraction procedure in the second to achieve a good recovery and purification of the valuable fissionable material. Our endeavours are concentrated therefore on a flow-sheet consisting of the following process-steps:

- Burning of the graphite-containing fuel elements
- Dissolution of the oxide residues

- Isolation of protactinium
- Thorium and uranium recovery and purification by a TBP-solvent extraction procedure
- Waste treatment and solidification

However, no final decision can be pronounced on the detailed lay-out of the individual process-steps at the moment, since in several cases alternatives are still under investigation. This is true for the burning, dissolving and partly for the extraction. The overall alternatives are characterized by the well-known THOREX-flow-sheets and the KFA/TBP 23/25-flow-sheet.

We intend to finish the flow-sheet design by the end of next year and to put a semi-technical pilot plant into operation in about 4 years from now. Design work on this pilot plant ("JUPITER") has started just recently. The size of the pilot plant will be rather small. It is limited by the output of the AVR-reactor and the hot cell facilities. The daily throughput being on the order of about 2 kg of heavy metal.

Time Schedule for HTGR - Reprocessing R & D-Work at KFA-Juelich

Fig. 1: Time Schedule for Reprocessing R & D-Work
at the KFA-Juelich

A continuous discharge of spent fuel spheres from the AVR-reactor is expected for the year 1972. In the meantime, the spent fuel elements must be stored in an interim storage facility to be erected at the reactor site. Larger quantities of spent fuel from the THTR-300 MW-reactor are not expected before 1977/78. A further increase should arise at the beginning of 1980.

Reprocessing of spent fuel elements from HTG-reactors will thus be adequately guaranteed at that time, if a head-end procedure is available, because the solvent extraction

separation and purification may be achieved if necessary in an existing PUREX-plant. We are aware that this less economic route represents only a transitional stage for the time being, when only a few thorium reactors are in operation.

We therefore started conceptual design studies on a 20 kg/day-combustion pilot plant just recently. It is aimed towards a decision which furnace type, e. g. shaft or fluidized bed oven, may be superior for the burning. Economic studies will reveal the best of one of the following combinations:

1. Final storage of the spent fuel elements at the power plant site.
2. Interim storage of the spent fuel elements at the power plant site and final storage in an abandoned salt mine.
3. Interim storage as well as burning of the spent fuel elements in a head-end burning installation at the power plant site and final storage of the oxide residues at the reactor site or an abandoned salt mine, respectively.
4. Interim storage as well as burning of the spent fuel elements in a head-end burning installation at the power plant site, but processing of the oxide residues in an existing PUREX-reprocessing plant (WAK or EUROCHEMIC).
5. Establishment of a burning head-end facility in an existing PUREX-reprocessing plant.

Combustion

For combustion of the graphite fuel the KFA is developing a fluidized bed combustion furnace.

The three principal problems are:

1. Crushing of fuel elements
2. Heat-control of the furnace
3. Off-gas clean up

The crushing of the fuel spheres is carried out in a hammer mill, which has been furnished with a 1,5 mm slot sieve.

The product of the hammer mill is fed into the burner via a screw conveyer. The feed rate is controlled by the fluid bed differential pressure. The bed temperature is controlled by outer heating and cooling coils at 750°C. Should temperature excursions occur, the feed-gas oxygen content, normally kept at 75 %, is decreased automatically. The off-gas is passed through sintered metal filters, equipped with an automatic blow-back device. To avoid local thermal hot spots just above the bottom flow plate in burning of graphite without the addition of alumina, which might cause sintering of the metal oxides, the burning gas must be diluted with CO₂. This is achieved by refluxing part of the off-gas.

An automatic controlled model of a fluidized bed furnace has been cold tested extensively and is being installed in the hot cell. The construction of a 10 " diameter furnace (2 kg heavy metal per day capacity) is in progress.

Hot cell experiments on the combustion of AVR-fuel spheres in a shaft furnace model (2 spheres per hour capacity) revealed that fiber glass filters retain the radioactive dusts and aerosols with decontamination factors of about $10^3 - 10^4$. For some isotopes like cesium and ruthenium, the decontamination factors are remarkably lower².

Also iodine, if present, passes these filters and can be retained to a great extent in charcoal traps. Because of the higher combustion temperature in the shaft furnace (about 950°C), the off-gas contamination is expected to be somewhat lower in the fluidized bed device. As a result of the first hot cell runs, a revised experimental program on the problems of off-gas clean up has been started.

The release of fission gases during the combustion of oxide fuel amounts to 1 - 5 % of the total amount present, whereas carbide fuel retains only a few percent of these fission gases.

Dissolution

The combustion produces uranium-thorium oxide mixtures or mixed uranium-thorium oxides depending whether carbide or oxide fuel particles were employed. For their dissolution boiling THOREX-reagent, a mixture of 13 M HNO_3 , 0,1 M $\text{Al}(\text{NO}_3)_3$ and 0,05 M HF, is well suited. In case of carbide fuel, the dissolution time amounts to about 1 to 4 hours depending on the burn-up and the thorium and uranium concentration aspired in the final solution. Fuel with a very high burn-up may leave behind an undissolved residue of a yet unspecified composition. If silicon carbide coated fuel particles have to be reprocessed, a small fraction of the silicon (0,5 to about 5 %) is found in solution afterwards. However, it seems to cause no serious problems in the extraction step. Some annoyance may arise from uncomplete burned material during the filtering and extraction steps.

Highly refractory mixed oxide particles require a much longer dissolution time, ranging from 6 to more than 50 hours. The reaction seems to be highly dependent on the composition, burn-up, surface area and density of the

particles. In order to explore the dissolution mechanism thorough kinetic investigations are in progress.

A rather rapid and complete dissolution of any kind of highly refractory oxide material may be achieved applying an acid-salt melt treatment, characterized by the following equations:

Systematic investigations proved that appropriate mixtures of $\text{K}_2\text{S}_2\text{O}_7/\text{K}_2\text{SO}_4$ require a minimum amount of melt material for a complete dissolution within 30 to 60 minutes at about 750°C ³. Part of the K_2SO_4 may be recovered in a later process step and reused, thus reducing the amount of solid waste. After pouring the liquid salt melt in the requisite amount of water, uranium dissolves as uranyl sulphate, whereas thorium is quantitatively precipitated as the sparingly soluble double salt $3,5 \text{K}_2\text{SO}_4 \cdot \text{Th}(\text{SO}_4)_2$ together with some fission product sulphates like Ba, Sr. This precipitate is washed free from uranium and thereby transformed into the compound $2 \text{K}_2\text{SO}_4 \cdot \text{Th}(\text{SO}_4)_2 \cdot 2 \text{H}_2\text{O}$, which is then separated, dried and directly stored. This method enables a simple thorium pre-separation and is part of the KFA/TBP 23/25-process flow-sheet.

Protactinium-Recovery

Short cooling times for thorium-containing fuel elements require a special procedure for the recovery of the ^{233}U -precursor, ^{233}Pa . The application of the well-known THOREX flow-sheet permits a complete recovery of thorium and uranium, but no isolation of the ^{233}Pa . The sometimes proposed MnO_2 -precipitation gives unsatisfactory results.

A thorough analysis of all published protactinium separation and purification procedures revealed that the sorption on Vycor glass columns shows the best promise for a technical realisation.

After preliminary investigations with tracer amounts of protactinium, a series of hot cell experiments with highly irradiated thorium fuel was performed in order to establish and extend the tracer experiments. It could be shown already in the first runs that this method offers a rather good protactinium recovery from THOREX-feed solutions giving decontamination factors for thorium, uranium, cesium and Zr/Nb of at least 10^3 , 8×10^2 , 4×10^2 and 6, respectively. These results are in agreement with those of GOODE and MOORE⁴.

Experiments have shown also that the sorptive Vycor glass separation procedure can also be applied in the sulphate⁵, and possibly in the chloride system^{6,7}.

Uranium Separation and Purification

Up till now, our hot cell experiments were dealing with the verification of the well-known THOREX- and INTERIM-23-flow-sheet. In general, it may be stated that we obtained rather satisfactory results⁸. The measured separation and decontamination factors are in good agreement with already published results based on work done mainly in the USA. There is no doubt that either one of the proposed flow-sheets can meet the requirements for a reprocessing of thorium-containing HTGR-fuel. On the other hand, it is our opinion that it would be worthwhile to develop improved flow-sheets. Such a possibility could be the KFA/TBP 23/25-Process.

The chemical flow-sheets of this process with the pertinent mass-streams are shown in figures 2 and 3.

Fig. 2: Chemical Flow-Sheet of the KFA/TBP 23/25-Process, Head-End

Fig. 3: Chemical Flow-Sheet of the KFA/TBP 23/25-Process, Solvent Extraction

The spent fuel elements are first crushed and then burned. Afterwards the resulting oxide residues are dissolved in a $K_2S_2O_7/K_2SO_4$ -mixture. The washed and dried potassium sulphato thorate precipitate retains less than 0,1 % of the total uranium input and may be directly stored. The filtrate, containing the uranium is adjusted to solvent extraction conditions by adding nitric acid. Details of the extraction procedure may be deduced from figure 3 and the literature⁹. The high acid concentration of 5 molar HNO_3 in the scrub solution promotes a good ruthenium decontamination. This is true also for Zr/Nb and Ce.

The proposed flow-sheet guarantees a uranium recovery of 99,9 % applying 6 theoretical extraction- and 3 theoretical backextraction steps. Since the applied mechanical stirred mixer settlers of the CEN-type exhibited a stage efficiency of 60 % only, the practical stage numbers needed were 10 and 5, respectively. Even better results may be obtained using air pulsed mixer settlers. In cold laboratory tests we could prove their superiority compared to the mechanically stirred ones.

Cold tests of the flow-sheet are complete and hot cell experiments are in preparation.

An unfortunate drawback of this process is the sulphate containing aqueous waste. However, we hope to be able to surmount this difficulty in our waste treatment efforts applying phosphate glasses for their solidification.

II. CHLORINEX-PROCESS

Next to the burning, the dry chlorination of the ground graphite-based fuel elements of HTG-reactors may be another advantageous method for reprocessing this type of fuel element. We are therefore developing an alternative flow-sheet to the burn/leach-TBP solvent extraction process calling it CHLORINEX-process.

Because all the carbon remains in the elementary state by this method, the off-gas treatment and especially the rare gas retainment, if necessary, is simplified considerably. It may be done by cold-trapping after removing the excess of chlorine by absorption.

No serious problems are encountered in case of processing silicon carbide containing fuel particles, since easily volatile SiCl_4 is formed and thus can be separated from the heavy metals.

The crucial problem of this process is the corrosion at elevated temperatures with chlorine in the presence of carbon or carbon monoxide and with chlorides. If we succeed in controlling the corrosion problems, the chlorination represents certainly very promising features for future application of reprocessing graphite containing fuel elements¹⁰.

The principles of the proposed flow-sheet are shown in figure 4.

Chlorinex - Process

Fig. 4: CHLORINEX-Process;
Schematic Process-Flow-Sheet

After an adequate cooling period, the graphite-based fuel elements are crushed and then ground in a gas tight combined milling device to a particle size between 50 and 300 μm with only a small fraction of fines below 50 μm . Thus all coated particles (with a diameter from 500 to 800 μm) are broken up.

The material is then fed into a fluidized or a fixed bed and chlorinated with elementary chlorine at about 1000°C. The carbon, which is present in large quantities, reduces the oxides. At temperatures around 1000°C reactions like the following occur at a high rate.

At this temperature the heavy metals and most fission products are volatilized. The remaining graphite may be discarded and stored as medium level waste or burnt in a special unit. No serious off-gas problem should arise in burning this graphite, because all the volatile fission products are already released during the chlorination step. The chlorides are condensed in a condenser unit. The thorium-uranium chloride condensate is dissolved in 4 M hydrochloric acid. This solution is passed through a silica gel or vycor glass column to adsorb and remove the protactinium.

The protactinium-free solution is further separated and decontaminated by a solvent extraction cycle. One alternative investigated is the conversion of the chlorides into nitrates and then applying the THOREX-flow-sheet; another possible method is a solvent extraction procedure in the chloride system with a solution of long chain aliphatic amines in aromatic hydrocarbons. The purified uranyl chloride or nitrate solution is used for the

production of particles with the sol-gel technique.

A thorium-uranium separation is theoretically possible by fractional condensation followed by a sublimation procedure¹¹. The uranium content of the condensed thorium chloride should be lower than 80 ppm corresponding to 0.2 % uranium loss. Despite extensive studies we did not overcome the technical difficulties of fractionating the condensates.

Crushing and Grinding

The graphite fuel spheres (or any other graphite fuel element type) containing coated fuel particles are first crushed in a hammer mill. In a typical experiment about 40 % of this product passed a 0,3 mm sieve. The oversize particles are then ground in a second step in an impact disc mill, thus assuring that every particle is broken up. A circulating air-flow device provides the steady removal of the fine-grain. This air flow can be adjusted and controls the amount of fines ($< 50 \mu\text{m}$) below 25 %. This is necessary, because otherwise the ground material shows bad fluidisation characteristics.

Chlorination and Condensation

The resulting ground material is then chlorinated in an apparatus schematically shown in figure 5.

Fig. 5: Mills and Chlorinator Reactor for
CHLORINEX-Process

The chlorination of the heavy metal oxides in the presence of graphite is an endothermic reaction. At 1000°C the reaction proceeds very quickly, yielding uranium recoveries of more than 99.8 % within 1 hour.

Though the reaction rates below 1000°C are still sufficiently high, this temperature seems to be the optimum, due to the following fact:

The lowest possible temperature of the reactor is

determined by the behaviour of the fission products. At 800°C, for instance, the vapour pressure of some fission product chlorides (Cs, Ba, Sr, RE) is very low. Though the main part of thorium and uranium oxide is volatilized after a short time, some uranium is retained as chloride by the fission products. Quantitative volatilization of the uranium was only possible, if most of the fission product chlorides were vapourized.

The chlorination reactor consists in principal of an inner porous graphite cylinder, directly heated by current passage, and an outer cylindrical and gas tight ceramic jacket. The material is continuously fed into the fixed bed reactor and flows slowly, as the reaction proceeds, from top to the bottom of the graphite cylinder. The gaseous chlorides of thorium, uranium, protactinium and several fission products penetrate the graphite walls into the outer ceramic tube and then leave it through a connecting pipe sideways.

The fixed bed reactor proved to be superior over the formerly favored fluidized bed conception since it works continuously and the danger of interference is much smaller even if the fine-grain fraction may increase considerably.

The condensation of all volatilized chlorides is achieved in a condensation unit, applying gas injection to gain a snow-type gas phase condensation. After long and laborious trials, this method avoids caking of the condensate on the walls securing a deposit of fine and free flowing powder.

Off-gas Treatment

The off-gas leaving the condenser contains besides excessive chlorine and carbon monoxide, the rare gases, silicon tetrachloride, tritium chloride and possibly small amounts of iodine and tellurium. This off-gas is scrubbed with an organic solvent, for instance hexachlorobutadiene. SiCl_4 and a small part of the chlorine can be absorbed in a rather small absorption column, the main part of the chlorine however, in a larger adsorption tower. The chlorine is then stripped from the solvent and fed back into the process. The SiCl_4 may be separated from the solvent by distillation under reduced pressure.

Excess chlorine may be eliminated by scrubbing in a packed column filled with soda lime.

WASTE EFFLUENT TREATMENT AND SOLIDIFICATION

Included in our reprocessing program is a high activity waste processing and interim storage facility. It will be operated in connection with a fuel element interim storage plant for the spheric AVR-fuel elements, the construction to be completed by the end of 1973.

The aqueous waste effluents of the "JUPITER"-plant will be treated and solidified for final storage in the facility with a daily throughput comparable with the capacity of the reprocessing pilot plant. The process flow-sheet considered is shown schematically in figure 6.

Fig. 6: Schematic Flow-Sheet for Waste Effluent Treatment and Solidification

The highly radioactive waste streams as well as flushing streams with low salt content are first concentrated in an evaporator equipped with a rectifying column. The low activity distillate (about 0,1 M HNO_3) may be reused in the reprocessing plant for rinsing, diluting, etc. or retreated and dumped. Evaporator, condenser and other parts of the plant are fabricated from titanium.

The concentrated solution is then denitrated applying formaldehyde, thus minimizing a ruthenium volatilization.

Simultaneously a further concentration of the solution is achieved. Finally, the resulting concentrate is passing a product cooler and collected in a storage tank. The liberated nitrous gases are reconverted into nitric acid, which is reused after concentration.

In the next step, the concentrated solution is fed on to a roller-dryer. The resulting dry flaky product is mixed with glass-forming admixtures and fused in inductive heated graphite crucibles and then poured in steel pots. Most likely, borosilicate or phosphate glasses will be employed.

By passing the process gas-streams from the different process steps through the denitrator, the gas will be purged of volatile fission products. The unified off-gases are washed, dried and released through the stack.

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DISCUSSION

V. C. A. Vaughen: Would you care to describe the decontamination factors you have found in your burning studies for the primary metallic filter, the secondary filters, and through the plant off-gas?

E. Merz: I didn't have the time to go into more detail in my presentation so I will give you a reference to the figures and numbers we obtained. Collaborate studies on this subject have been made and are still under way together with our industrial partner, the Nukem Company. Reference 2 of our paper describes the results obtained so far in detail.

L. R. Zumwalt: In your clorinex process, of what materials is the chlorination reactor made? Do you have any corrosion problems incident to the chlorine treatment carried out at 1000°C?

E. Merz: The chlorination reactor consists of an inner-porous cylinder made of graphite and an outer gas tight jacket. The graphite itself withstands chlorine very well at 1000° C. Practically no corrosion could be observed during long reaction times. The problem is the outer gas-tight cylinder. Metals are not available for use at this high temperature. Porcelain or even better graphite tubes are well suited for this purpose. Their safe application must still be proven, however, in hot runs under severe conditions.

A. M. Weinberg: How completely do you expect to retain radioactive gases that come from your initial burning of the fuel elements?

E. Merz: Our clean-up system should provide a nearly complete retention of radioactive gases. Dusts and aerosols are respectively absorbed on a stack of fiberglass filter and charcoal traps. Tritium is oxidized to T_2O during the burning and this can be removed from the gas stream by absorption on molecular sieves. In case a rare gas containment is aspired, all the carbon dioxide is first absorbed on large zeolite columns, whereas the unpolar rare gases pass these columns without adsorption and may be coldtrapped and stored. The zeolite can be easily regenerated by heating up. The nearly radioactive free carbon dioxide is then released into the open air. However, this method is not a cheap one.

M. Dalle Donne: Coming back to the question of Dr. Weinberg of burning of the graphite. I think that this is a critical point. If you have all the graphite of the balls, the amount of CO_2 produced must be enormous, so that you must discharge the CO_2 to the atmosphere and you cannot store it. You should, therefore, be absolutely sure to eliminate the major part of the radioactivity from the CO_2 including the activity coming from the noble gases.

E. Merz: I agree with your comment. This problem represents certainly one of the main drawbacks of the combustion procedure. On the other hand, a nearly complete clean-up of the large off-gas volumes is feasible, though it is an expensive approach. That's the main reason why we are trying to develop the Chlorinex process. Here the off-gas clean-up is simplified considerably due to the fact that only small quantities of gases are formed.

A. L. Lotts: Would you comment on what you intend to do in the future with the U235 which has been irradiated?

E. Merz: To my knowledge we have to deal with a used feed and breed element, and so in this case we are recovering both the unconsumed 235U as well as the bred 233U. In case a feed and breed concept is applied the fuel element may just be dumped since most of the 235U has disappeared. Depending on the 235U/236U ratio it may be recovered and re-used. Maybe the experts could better answer this question.

C. B. Zitek: What is the initial carbon burning step being planned to be done at the power plant?

E. Merz: This is proposed for initial operation when only a very few HTG-reactors are in operation. Later the head-end facility will be integrated, of course, in the actual reprocessing plant. The conceptual design study may even reveal that this concept doesn't give any advantage at all. It depends largely on the transportation costs.

E. Merz: Dealing with the question of reliability of the heavy machinery needed for the grinding of graphite base fuel elements, one of the most important requirements in designing the layout of the crushing and milling apparatus is good reliability. In our experiments we found

only a small abrasion rate of the heavy duty parts. The machinery is constructed such that spare parts can easily be replaced by remove operation. Further, the operation and maintenance is very simple.

R. E. Blanco: What type of waste denitration and solidification process did you select and why did you select this method?

E. Merz: The proposed flow-sheet for the denitration and solidification is a result of a thorough study and evaluation of the different methods developed and used elsewhere. Considering the properties of the expected waste solutions as well as the special requirements for a final disposal in Germany, it seems to us to secure in an optimum way the highest versatility, safety, simplicity, and economy. The results obtained so far in our experiments justify our decision. Further, the dried potassium sulphate thorate precipitate is foreseen for a direct storage; it will probably never be reused.

By Title

Review of USAEC - ORNL

HTGR Fuel Recycle Program

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SOME FEATURES OF THE LOW ENRICHMENT HTR FOR
VARIOUS FUEL ELEMENT DESIGNS.

PART 1.

Parametric Fuel Cycle Surveys

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Introduction

In the course of their present and previous work for the optimization of low enrichment fuel cycles for HTR's, the authors have tried to assess the fuel cycle cost in terms of very few parameters which have a paramount effect on the physical performance of the fuel. In this paper an attempt is made to pinpoint such "predominant" parameters which will allow, on the basis of their choice alone, to make a good prediction of the physical and economical performance of the fuel considered.

This work is based on a number of fuel cycle surveys carried out at Ispra in recent times, the most recent with four fuel element concepts: the hollow rod, the tubular, the teledial and the trefoil fuel element. They cover about all the fuel element types considered up to now in Europe for low enrichment fuel cycles in HTR's, with the exception of the multi-annular fuel element put forward in the early stages of the studies on low enrichment cycles and now abandoned.

The survey described here was performed for two values of the core-average power density, varying the burn-up and the moderation ratio within the range of interest. The variation of C/U238 was obtained by varying the heavy metal density, while the geometry (fuel dimensions and lattice pitch) was kept constant. This simplified approach was adopted for the present survey, as our previous work¹ had shown that the whole range of interesting values of the parameters which determine the fuel cycle physics and economics can be covered in this way.

Indeed, two fuel elements of quite different size, shape and heavy metal density lead to the same enrichment, conversion ratio and age factor provided the moderation ratio (C/U238) and the resonance integral are the same. This will be explained in more detail later on.

Fuel Cycle Physics

The Calculation Method.--The parameters of a fuel cycle for a given power density, buckling and discharge burn-up were evaluated for the equilibrium conditions with the hypothesis of continuous charge-discharge. The code MOGA² (MODified GAffee) was used. It calculates the necessary enrichment of the fresh fuel and the composition of the discharged fuel.

No recycle of the spent fuel was assumed, with sale of the produced Plutonium. The calculations were carried out with 20 energy groups (8 fast).

The variation of the effective cross sections with the fuel composition was taken into account by means of variable selfshielding factors.

The dependence of the selfshielding factors upon the macroscopic cross section of the cell was fitted with a simple formula to a number of cell calculations performed with the SN code WRETCH³ (incorporating WDSN).

For the U238 resonances, the effect of the double heterogeneity of the fuel was taken into account by the equivalence theorem:⁴

$$\sigma_{m \text{ eff}} = \sigma_{po} + \frac{\sigma_m + \frac{\alpha_e}{\ell N_e}}{1 + \frac{N_o \ell_o}{\alpha_o} \left(\sigma_m + \frac{\alpha_e}{\ell N_e} \right)}$$

where

N_o = absorber density in the kernel

ℓ_o = mean chord length of the kernel

α_o = Bell factor of the kernel

σ_m = carbon scattering cross section per absorber atom in the lump

N_e = absorber density in the lump

ℓ = mean chord length in the lump

α_e = Bell factor of the lump

σ_{po} = potential scattering cross section of the kernel per absorber atom

The mean chord length ℓ was determined with the Leslie approximation

$$\frac{1}{\ell} = \sum_f \frac{\ell_{im}}{\rightarrow \infty} (1 - P_{ff}) \sum_f$$

where

P_{ff} = lump-to-lump collision probability determined by the collision probability code Procope⁵

$\sum_f$ = absorption cross section of the lump

The Bell factor α_e was determined as a function of σ_m and $1/\ell N_e$ by fitting the above-mentioned formula on a number of σ_m eff calculations performed with a ZUT code, modified to take into account the double heterogeneity according to the Nordheim-Lane theory.⁶

The treatment of fission products was quite detailed: 25 individual fission products and two aggregates were considered, so that the error in reactivity induced by this treatment as compared to the exact one was estimated to be in any case smaller than 0.2%.

Main Assumptions.--As said, the fuel cycle calculations were performed in a zero-dimensional scheme. Therefore, the reactor size affects the fuel cycle physics only through the buckling, which was assumed constant throughout the energy range and was varied parametrically in the survey.

The equilibrium K-effective was assumed to be 1, i.e. no additional reactivity investment in control rods was assumed, apart from that already devoted to Xe-override.

The charge and discharge of the reactor were assumed to be continuous, therefore no account was taken of the loss of neutrons due to the reactivity variation during a reload interval. Also, no account was taken of the approach-to-equilibrium phase of reactor operation. This point is discussed in part 2 of this paper.

These approximations are quite satisfactory in many instances, but particularly so if the results of the present work are used to compare different types of fuel and to have an insight into the effect of various

physical parameters, rather than to make a detailed assessment of the fuel cycle cost for a particular case.

Cross Section Data.--The Gulf General Atomic cross section library was used, in connection with the code GGC II (GAM - GATHER Combined).⁷

Age Factors.--The age factor is defined here as the ratio between the power in a channel loaded with fresh fuel and the power in that channel loaded with the equilibrium (life-average) composition.

As it is calculated by the MOGA code in a zero-dimensional scheme, it does not take into account the space variation of the flux following the reload of a channel, let alone of a fuel block of, say, 18 channels. This leads to an underestimate of the age factor particularly at low age factor values. However, this approximation was considered to be satisfactory, because the purpose of this survey is to show:

- (a) the little effect of the choice of the shape of the fuel on the fuel cycle physics
- (b) the dependence of, among others, the age factor upon the effective moderation ratio (see list of symbols).

Fuel Cycle Economics

The present-worth accounting method was not used, as only the equilibrium conditions of the fuel cycle were known.

The fixed fuel cycle costs, i.e. those depending on the core inventory of fuel, were assessed by attributing to the core a "value" equal to the average between a core full of fresh fuel elements (including the fabrication cost) and a core full of spent fuel elements (deducing the reprocessing cost). The calculations were performed for a continuous charge-discharge equilibrium cycle with enriched uranium feed and sale of the discharged Plutonium and Uranium.

Two sets of values were assumed for the fabrication costs and for the discharged plutonium value: the low fabrication cost being coupled with the low plutonium value and vice-versa. The high fabrication cost is representative of the prototype realisations, while the low estimate is characteristic of a large market. These two sets imply different cost

figures for the different fuel elements which will be described in the next chapter. Other calculations with economic assumptions identical for all fuel elements were also performed (see Chapter 5). The relevant economic assumptions are listed below:

Plutonium value:

8 (low) and 15 (high) \$/g fissile

Fabrication cost:

$$F = A + B K + D \left(\frac{C}{U} \right) \text{ \$/Kg uranium}$$

where

K = cost of UF_6 (\$/Kg uranium)

$\frac{C}{U}$ = atomic ratio carbon to uranium (moderation ratio)

K accounts for fabrication losses, D for graphite fabrication cost. The values are given in Part II, Table 2.

Reprocessing cost: 60 \$/Kg

Plant data:

gross thermal efficiency	42%
blowers efficiency	90%
electr. motors efficiency	95%
external pressure-drop	0.3 at.

Reference pressure drop for the four fuel elements, assuming an equivalent triangular pitch of 8.87 cm:

	core-average power density w/cm ³	core-pressure drop atm
Fuel element 1	1	0.07
Fuel element 2	7	0.11
Fuel element 3	7	0.17
Fuel element 4	7	0.14

These reference values are inclusive of the radial power shape factor, not of the age factor. They were therefore multiplied by the square of the age factor for each single case.

Delays:

Out of core residence time for the reprocessed fuel 360 days
 fresh fuel storage 180 days
 interest rate = 11%

Fuel Cycle Survey with Four Fuel Elements

The results of the survey reported here will show the little importance of the fuel element shape as far as the fuel cycle physics is concerned. This leads to plotting the results in terms of very few parameters, as it is shown in Chapter 5.

The fuel elements considered here are the four described in Fig. 1: in this figure the cross section of a single channel is shown. Each graphite block would contain a number of the channels. The fuel matrix is shown by dotted areas. The fuel element 1 has only one (external) coolant channel. Fuel elements 2 and 3 have internal and external cooling. The graphite density was assumed to be:

1.8 g/cm³ for the cans

1.6 g/cm³ for the bulk

No account was taken of the reduction of the effective graphite density because of control rod holes etc. This correction would not have been meaningful for our investigation, particularly so if one thinks that no reduction of the uranium loading was assumed to account for the axial distance between the fueled sections of the channel.

The resulting relationship between heavy metal density in the fuel matrix, moderation ration C/U8 and effective moderation ratio (C/U8)_{eff} (see below) is reported in Fig. 2 for an equivalent triangular lattice pitch of 8.87 cm.

The calculations were performed for:

- 2 values of the core-average power density: 6 and 8 w/cm³ (4 and 5 for element 1)
- 3 values of the average burn-up at discharge: 60, 80, 100 Mwd/Kg
- 4 values of the moderation ration C/U238: 200, 250, 300, 400
- 1 value of the Xe override requirement: 60% of nominal power

A constant value of the equivalent triangular pitch was used: 8.87 cm. Indeed, previous experience had shown that a parametric variation of the pitch would not contribute any additional information concerning the fuel cycle. This is satisfactorily described by moderation ratio and resonance integral only, as far as the effects of lattice heterogeneity are concerned. In other words, the effect of a pitch variation can be simulated with a variation of fuel geometry and heavy metal density which lead to the same resonance integral and moderation ratio. We shall dwell on this point with more detail in the next chapter.

As for the effect of power density on the leakage, a one-group Buckling was deduced from a simplified analytical treatment for a flattened reactor. A more detailed study would not have much sense, because of the other approximations involved in zero-dimensional assessments and because a careful assessment of the Buckling would be possible only for a detailed reactor design.

The following values were used:

power density:	4	5	6	8	w/cm ³
Buckling:	.52	.59	.65	.77	m ⁻²

The equilibrium fuel cycle assessment was performed with the method described in Chapter 2. The moderation ratio C/U_8 was varied by changing the heavy metal density in the compacts.

As an example of the results, the relative fuel cycle cost* is plotted for one case in Fig. 3 to show how flat is the behaviour of the cost as a function of burn-up and moderation ratio. On the basis of graphs of this type the optimum cases reported in Table 1 were chosen. Figure 3 shows that even a considerable change of burn-up or moderation ratio wouldn't affect the values of Table 1 significantly. The values are given relative to a reference case (one for each economic assumption) which is quoted with 100 in the table.

* Note. Throughout this paper we have followed the policy of plotting relative or differential fuel cycle costs, not absolute values. This was made not in order to conceal results, which can be easily deduced from the information provided, but because giving absolute costs was nor the purpose neither the most significant aspect of this paper.

The differences between the four elements in Table 1 are essentially due to the different economic assumptions.

To show how little the geometry of the fuel affects the fuel cycle cost, the relative cost was plotted in Fig. 4 for a single set of economic assumptions (those corresponding to low cost, fuel element 2, see Chapter 3).

Figure 4 shows clearly that the fuel cycle economics is practically the same, for different geometries, provided the moderation ratio $C/U8$ is the same. This depends upon the fact that:

- the fabrication and reprocessing item of the fuel cycle cost depend only on the burn-up
- the graphite fabrication item depends only on $C/U8$ and burn-up
- the fuel consumption item is not very much affected by the fuel geometry, i.e. resonance integral, because this has counteracting effects on the average enrichment and on the conversion ratio. Indeed, in these reactors which are optimized at high burn-up, full advantage can be taken of any improvement in the conversion ratio. As a consequence, the increase of average enrichment corresponding to an increase of the resonance integral is compensated by an increase of the conversion ratio, which leaves the feed fuel enrichment about the same, therefore the fuel consumption about the same, for different geometries.

These considerations on counteracting effects suggest that the same compensations might not occur for other fuel cycle parameters, like enrichment, conversion etc. This is true, and it was found that a wide scatter of the results for different geometries would occur, if a plot of conversion, enrichment, age factor was attempted versus the moderation ratio $C/U8$. Therefore, the effective moderation ratio was introduced, to account for both moderation and resonance integral. The effective moderation ratio is the physical moderation ratio where the $U238$ atoms are weighted with the resonance integral. Having chosen 60 as a reference value for the R.I., the effective moderation ratio was defined as

$$\left(\frac{C}{U8}\right)_{\text{eff}} = \frac{C}{U8} \frac{60}{RI}$$

Figure 5 shows the significant physics parameters of the fuel cycle, plotted as a function of the effective moderation ratio for the various geometries at BU = 80 Mwd/Kg, 60% Xenon override. The power density is 6 W/cm³ except for fuel element 1 (5 W/cm³). However, the power density has very little effect on the plotted data (flux level, Xe absorption). This figure shows how remarkably well any lumping effect (geometry, heavy metal density) can be described in terms of the effective moderation ratio only.

A Generalized Parametric Representation of the Results

With the above-mentioned results in mind, a parametric representation of the fuel cycle data was envisaged, which could allow the HTR designer to deduce the essential information concerning the fuel cycle physics by interpolation between a few curves. The following "main" parameters were chosen:

burn-up (BU) = 60, 80, 100 Mwd/Kg

buckling (B^2) = 0.5, 0.9 m⁻²

effective moderation ratio ($C/U8 \times 60/RI$) = 200, 250, 300, 400

Besides these main parameters, other three "secondary" parameters are needed to define the fuel cycle physics, i.e.:

$C/U8$ moderation

$\bar{\gamma}_v$ power density

φ_c core-average carbon density

The use of the moderation ratio $C/U8$ is obviously equivalent to that of the specific power $\bar{\gamma}_p$ in the fuel, as they are proportional at constant power density (at 7 w/cm³, the moderation ratios 200, 300, 400, 500 correspond to specific power 50, 75, 100, 125 $\frac{W}{g}$ respectively).

It should be pointed out that if $C/U8$ can be regarded as a secondary parameter from the point of view of the physics (see e.g. Fig. 14) the same cannot be said about economics. The important effect of $C/U8$ on economics is obvious if one thinks of its equivalence to specific power.

Figures 6 and 7 are reported to show this effect. They were calculated with the physics data of Figs. 8-19 and the economic assumptions corresponding to fuel element 2, low cost.

The effect of the "main" parameters and of C/U8 on the fuel cycle physics is shown in Figs. 8 to 19. They can be used to interpolate the results for any fuel cycle, whatever the size, shape and density of the fuel element.

The reported data are relative to

- power density 7 w/cm^3
- core-average graphite density 1.4 g/cm^3

However, the effect of these two "secondary" parameters on the fuel cycle physics is small, as it is due only to

- flux level (X_e absorption) : $\bar{X}_v$ and $\bar{l}_c$
- neutron migration area (leakage): $\bar{l}_c$

Therefore the figures 8-19 can be used for a wide range of $\bar{v}_v$ and $\bar{l}_c$. For a more detailed analysis, corrections factors could be elaborated.

The effect of C/U8 on the fuel cycle physics, illustrated by Figs. 8-19, is more clearly shown in Table 2.

The meaning of the symbols is as follows:

- η eta value for the mixture of fissile isotopes
- c carbon absorption
- X_e Xenon absorption
- $X_{e \text{ ov}}$ absorption by override control rods
- FP fission products absorption
- NFHM absorption by non fertile heavy metals (U236, 237, 239 twice, Pu242)

In this table the neutron balance is reported for a given set of the "main" parameters and several C/U8 values.

- on the thermal neutron spectrum (η)
- on the ratio between thermal and epithermal flux (NFHM, FP)
- on the carbon absorption, as obvious (c)
- on the flux level, as it is proportional to the specific power (X_e , $X_e \text{ overr.}$)
- on the leakage, because of the variation of the migration area, due to the variation of U238 density, enrichment etc. at constant graphite density.

Concluding Remarks

The heterogeneity of the HTR cores cannot be as large as to make the fuel element shape and size relevant from the point of view of the fuel cycle physics. The designer has therefore a large freedom of choice of the fuel shape, size and density and lattice pitch, as far as neutron physics is concerned. Also, the thermal optimization of the fuel element can be carried out independent of the nuclear optimization. The flexibility of the HTR design makes the use of such generalized graphs as those presented here particularly useful.

Acknowledgements

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List of the Main Symbols and Definitions

$\bar{\gamma}_v$ power density ($\frac{W}{cm^3}$) = power per unit core volume

$\bar{\gamma}_p$ specific power ($\frac{W}{g}$) = core-average power per unit heavy metal weight

$\bar{l}_c$ core-average carbon density ($\frac{g}{cm^3}$)

$(\frac{C}{U8})$ effective moderation ratio = C/8 with the U238 density eff weighted with the resonance integral

BU average burn-up at discharge (MWd/T_{hm})

$(\frac{C}{U8})_{eff}$ effective moderation ratio = C/U8 with the U238 density weighted with the resonance integral

RI resonance integral (b_n) = U238 absorption cross section averaged over the whole epithermal range of lethargies

B² buckling (m⁻²) constant over the whole energy range and introduced in the zero-dimensional burn-up code to account for neutron leakage.

X age factor = reactor-average ratio of the beginning of life to the life-average power in a fuel element

$\bar{C}$ conversion ratio = core average (life-average) ratio between rate of formation and rate of destruction of fissile isotopes. Evaluated for the equilibrium composition

$\bar{\eta}$ eta value (neutron regeneration factor) for the mixture of fissile isotopes. Evaluated for the equilibrium composition

ϵ enrichment = $\frac{\text{fissile material weight}}{\text{total heavy metal weight}}$ (%)

ϵ_i fresh fuel enrichment (%)

$\bar{\epsilon}$ average fuel enrichment (U235 + fissile Pu) (life average = core average) (%)

$\epsilon_{f \text{ or}}$ total enrichment (U235 + fissile Pu) of spent fuel (%)

$\epsilon_{f \text{ pu}}$ spent fuel enrichment in fissile Plutonium (%)

X % Xenon override requirement: capability of restart after sudden power reduction from 100% to (100-X) %

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Figure 1.

The four fuel elements

Table 1. Minimum Relative Fuel Cycle Cost with Four Fuel Elements

Type of Fuel		1		2		3		4	
Power Density (W/cm ³)		4	5	6	8	6	8	6	8
Low fabri- cation cost	60% Xe override	105	101	102	100	103	101	107	105
	100% Xe override	107	103	105	103	106	105	111	110
High fabri- cation cost	60% Xe override	108	104	104	100	104	100	108	105
	100% Xe override	110	106	106	103	106	104	111	108

For All Types of Fuel		
	Optimum Burn-up MWd/Kg	Optimum C/U8
Low cost	80	300
High cost	100	300

Table 2. Effect of the Moderation Ratio on the Neutron Balance Equilibrium Fuel Cycle, Continuous Charge-Discharge

$$\text{Burn-up} = 100 \text{ MWd/Kg: } \frac{C}{U_{8 \text{ eff}}} = 250; B^2 = 0.5 \text{ m}^{-2}$$

power density = 7 W/cm³
core-average carbon density = 1.4 g/cm³

		Absorptions (%)						
$\frac{C}{U_8}$	$\bar{\eta}$	Leakage (%)	C	Xe	Xe 60 (%) override	NFHM	F P	Total Losses (%)
200	1.857	1.98	0.71	1.42	0.17	2.23	7.753	14.263
300	1.882	2.17	1.09	1.77	0.41	1.46	6.890	13.79
400	1.894	2.34	1.45	1.96	0.64	1.08	6.312	18.782
500	1.901	2.48	1.79	2.08	0.84	0.86	5.895	13.945

Figure 2. Matrix heavy-metal density and effective moderation ratio as a function of moderation ratio (triangular pitch 3.87 cm.)

Figure 3. Example of variation of fuel cycle cost with burn-up and moderation ratio

Figure 4.

Example of the effect of the fuel shape on the fuel-cycle cost

Figure 5.

Fuel cycle parameters as a function of the effective moderation ratio

$BU = 80 \text{ MWd/Kg}$
 60% Xe override

Figure 6.

Effect of the moderation ratio and of the effective moderation ratio on the fuel cycle cost

Figure 7.

Fuel cycle cost as a function of burn-up and effective moderation ratio, according to Figure 6

CU = optimum

Figure 8. Enrichment $BU = 60 \text{ MWd/Kg}$, $B^2 = 0.5 \text{ m}^{-2}$

Figure 9. Enrichment $BU = 60 \text{ MWd/Kg}$, $B^2 = 0.9 \text{ m}^{-2}$

Figure 10. Enrichment $BU = 80 \text{ MWd/Kg}$, $B^2 = 0.5 \text{ m}^{-2}$

Figure 11. Enrichment $BU = 80 \text{ MWd/Kg}$, $B^2 = 0.9 \text{ m}^{-2}$

Figure 12. Enrichment $BU = 100 \text{ MWd/Kg}$, $B^2 = 0.5 \text{ m}^{-2}$

Figure 13. Enrichment $BU = 100 \text{ MWd/Kg}$, $B^2 = 0.9 \text{ m}^{-2}$

Figure 14. Conversion ratio C $BU = 60 \text{ MWd/Kg}$, $B^2 = 0.5 \text{ m}^{-2}$

Figure 15. Conversion ratio C $BU = 60 \text{ MWd/Kg}$, $B^2 = 0.9 \text{ m}^{-2}$

Figure 16. Conversion ratio C BU = 80 MWd/Kg, $B^2 = 0.5 \text{ m}^{-2}$

Figure 17. Conversion ratio C BU = 80 MWd/Kg, $B^2 = 0.9 \text{ m}^{-2}$

Figure 18. Conversion ratio C $BU = 100 \text{ MWd/Kg}$, $B^2 = 0.5 \text{ m}^{-2}$

Figure 19. Conversion ratio C $BU = 100 \text{ MWd/Kg}$, $B^2 = 0.9 \text{ m}^{-2}$

SOME FEATURES OF THE LOW ENRICHED HTR
FOR VARIOUS FUEL ELEMENT DESIGNS

Part II

Comparison of the fuel element designs

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GHH	: Gutehoffnungshütte	- Sterkrade	
CCR	: EURATOM	- Ispra	

ABSTRACT

This part is devoted to the analysis of various types of fuel on the basis of the parametric study outlined in Part I.

Frame of work

The work is based on a survey of the fuel cycle cost with four fuel element concepts analysed in a companion paper to the present symposium¹: the hollow rod, the tubular, the teledial and the trefoil fuel elements. They cover about all the fuel element types considered up to now in Europe for low enrichment fuel cycles in HTR's, with the exception of the multiannular fuel element put forward in the early stages of the studies on low enrichment cycles and now abandoned. Some data are also given on the integral fuel block concept.

The present survey covers :

- two power densities for each fuel element : 6 and 8 MW/m³ for all but the hollow rod, for which 4 and 5 MW/m³ are considered ;
- three burn-ups : 60, 80 and 100 GWd/t ;
- two choices for the Xenon override requirements : 70 % and 100 % ;
- a range of moderation ratios from 200 to 400 C atoms/U 238 atoms, obtained by varying the heavy metal density, while the geometrical characteristics of the fuel (dimensions and pitch) are kept constant

within each design. This simplifying approach was selected for the present survey, since earlier work has demonstrated that the modification of fuel rod dimensions or pitch within practical limits only induce secondary effects ;

- two sets of assumptions for the fabrication costs and for the discharged plutonium value : the low fabrication cost being coupled with the raw plutonium value and vice-versa. The high fabrication cost is representative of the prototype realizations, while the low estimate is characteristic of a large market.

The calculations were done for a continuous charge-discharge equilibrium cycle with enriched uranium feed and sale of the discharged plutonium and uranium.

Fuel cycle cost

The fuel cycle costs are assessed for the four elements considered, the two power densities and the two Xenon override requirements and for two cost assumptions. Under the cost assumptions described in the paper, the fuel cycle was found to be 18 % lower for the low fabrication cost assumption than for the high fabrication cost assumption.

Generating cost

The equilibrium fuel cycle cost as assessed in this study is not a sufficient criterion for the selection of the fuel design and the fuel cycle characteristics.

The fuel cycle cost should first be corrected for the approach to equilibrium period. The influence of this period is illustrated.

Furthermore, the capital cost may be influenced by some fuel characteristics such as the power density. This effect is assessed for the four element designs.

Conclusions

The results of the fuel design and development efforts^{1,2} impose to exclude some areas covered by the present study. This pinpoints topics for further development.

INTRODUCTION

The purpose of this second part is to compare various fuel designs, described in a companion paper to the present symposium¹ and to emphasize the influence of the economical environment. The broad field to be covered leads to the adoption of simplified approaches, backed by previous experience ; some assumptions will be mentioned in the paper to illustrate this.

In this frame, the generalized parametric approach, outlined in Part I, is a perfect basis for more elaborate developments.

The study summarized in the present paper is sufficient to screen the most promising fields. Thereafter, it is necessary to look into more details, in order to select a near optimum design for each particular case. This detailed treatment can no more be limited to economic considerations, but has to be intimately connected with all the design, manufacturing and development aspects, some of which being described in other papers^{1,2}.

To avoid misleading figures and to give a clearer view of the importance of each item, the costs are being quoted in relative terms ; this shows better whether some trends are universal.

DESCRIPTION OF THE FUEL ELEMENTS

As explained in the paper already referred to¹, five fuel element designs were investigated, the hollow (HR), tubular (Tu), trefoil (Tr) and teledial (Te) rods and the integral block (Bl). The latter is similar to the Fort St Vrain fuel, while the four ones first mentioned have fuel rods independent of the structural graphite, as in the DRAGON and the PEACH BOTTOM reactors. The various fuels are sketched in Fig. 1 and their geometrical characteristics are given in Table 1.

Table 1. Characteristics of the various fuels considered

		Hollow rod	Tubular rod	Trefoil rod	Teledial rod	Integral block
<u>Fuel compact</u>						
Inside diameter	mm	25.4	39.5	-	-	-
Outside diameter	mm	42.0	54.4	19.0	11.8	12.7
<u>Graphite sleeve</u>						
Inside diameter of the inner coolant channel	mm	-	29.5	-	18.6	-
Outside diameter of the sleeve (fins excluded)	mm	52	64.4	29.0	58.8	-
Length	mm	500	500	500	500	-
<u>Graphite block</u>						
Nr. of rods per block		36	36	108	36	210
Nr. of coolant channels		18	18	18	18	108
Channel diameter	mm	63.4	74.8	41.0	67.0	15.9
Dimensions across flat	mm	377	422	375	385	361
Length	mm	1,000	1,000	1,000	1,000	793
Ng/Nu for HML = 1.0 g/cm ³		236,5	230	230	270	130

The graphite density was assumed to be 1.8 g/cm³ and the moderation ratio was varied in the survey by changing the heavy metal loading (HML i.e. the uranium density in the fuel region) from 0.6 to 1.4 g/cm³ (Fig.2). Earlier studies have shown that freezing the dimensions for a survey like this is acceptable, since the size ranges left open by other design limitations are quite narrow.

For each design, an equivalent triangular pitch was calculated taking into account the special blocks with the control rod channels and the edge features in all blocks. The resonance integral (Fig. 3) was calculated for such an idealized lattice. For each fuel type, the resonance integral depends, in decreasing order, on the heavy metal loading, the thickness of the fuel and the pitch. As a guide-line, the sensitivity to a 10 % variation of the parameter is :

- 2.0 barns for the heavy metal loading
- 2.0 barns for the fuel section thickness
- 1.5 barn for the pitch.

The design limits are such that the pitch (or block size) and the fuel thickness vary usually simultaneously for the tubular, teledial and trefoil; the effects on the resonance integral are then partially compensating each other. It can be considered for all practical purposes that resonance integral cannot be varied by more than 5 barns between extreme cases within each type of design, at a fixed heavy metal loading. The approximation of freezing dimensions is therefore not too bad, but certainly worth noticing. It is of the same order of magnitude as the difference in the values of the resonance integral computed at different places on the same idealized lattice. Furthermore, a systematic error might be introduced by the simplified representation, since most rods are at a distance of the majority of their neighbours by less than the equivalent pitch. Only when the results of representative physics experiments will be fully assessed, will it therefore be the time to broaden surveys as the one described in the present paper and to include variations in dimensions. The benefit of earlier surveys as this one will have been to restrict the fields to be considered.

METHODS AND ASSUMPTIONS

The fuel cycle characteristics were determined using the methods outlined in Part I.

The parameters which are varied in the survey are the following ones :

- C/U : from 150 to 300 approximately
- burn-up : 60, 80 and 100 MWd/kg
- power density : 6 and 8 MW/m³ except for the hollow rod for which 4 and 5 MW/m³ are considered.

As already mentioned in Part I, the fuel cycle characteristics are determined for an equilibrium fuel cycle, with continuous charge-discharge and no reactivity investment other than that needed for Xenon override capability. Two assumptions for Xenon override capabilities are considered, allowing power reductions of respectively 100 % and 60 % from nominal power level.

The fuel cycle cost is evaluated for the equilibrium fuel cycle for two economic contexts, differing essentially by the fabrication cost and the plutonium value. One context is representative of the near future situations which would be faced by the first power plants to be built : the fabrication cost is high because the plants are small and the research and development charges still significant ; on the other hand, the plutonium price (15 \$/g) is representative of its present scarcity.

The second economic context would be more valid from the end of the 70's on ; the fabrication cost is lower (approximately 50 % of the former), because research and development charges are amortized and because the fabrication plant has increased in size in order to serve a large number of power plants ; on the other hand, the plutonium price (8 \$/g) has dropped to its value for recycling in water cooled reactors.

The fabrication cost, including the cost of uranium losses and the interest charges during fabrication can be expressed as :

$$F = A + B k_u + D \left(\frac{C}{U} \right)$$

where k_u is the price of uranium as UF_6 (\$/kg U). The coefficients A, B and D are given at Table 2 for the four fuel elements and the two economic contexts considered (the fabrication cost is normalized to 100 for the tubular elements at 6 % enrichment and $C/U = 230$).

Table 2. Values of A, B and D

<u>Fuel element</u>	A	B	D
Hollow rod	30	1.9×10^{-2}	0.19
Tubular	28	1.9×10^{-2}	0.25
Teledial	35	1.9×10^{-2}	0.16
Trefoil	28	1.9×10^{-2}	0.26

The other main economic parameters are the following ones :

- Plant load factor : 0.75
- Uranium ore cost : \$ 8/lb U_3O_8
- Separative work cost : \$ 26/kg
- Tail assay of enrichment plant : 0.2 %
- Reprocessing cost : \$ 60/kg
- Interest rate : 10 %
- Net efficiency : ~41.5 %.

The influence of the pumping power on the net plant efficiency has been taken into account for each case.

The core pressure drop and the pumping power (Table 3) have been determined on the basis of the following assumptions :

- Reactor height : 600 cm
- Power shape factor : 1.3
- Perfect gagging of the coolant flux in order to keep the outlet temperature constant.

In fact, the effect of the blower power on the net plant efficiency is rather small, but of course it has also an influence on the capital cost of the plant, via the cost of the blowers themselves.

Table 3. Influence of the design and the power density on the pumping power

	HR		Tubular		Teledial		Trefoil	
Power density (MW/m ³)	4	5	6	8	6	8	6	8
Relative core pressure drop	25	39	56	100	48	84	58	104
Relative pumping power	62	72	68	100	63	90	68	100

FUEL CYCLE PERFORMANCE

Effect of C/U_8

As explained in Part I, the effect on the moderation ratio is mainly described by the effective moderation ratio, i.e. the moderation ratio corrected by the actual resonance integral :

$$\left(\frac{C}{U_8} \right)_{\text{eff.}} = \left(\frac{C}{U_8} \right) \cdot \frac{60}{RI}$$

The effective moderation ratio is plotted on Fig. 4 versus the actual moderation ratio for the four fuel elements. One can see that, for a same C/U_8 , the effective moderation ratio is significantly higher for the tubular and the hollow rods than for the trefoil and teledial.

For all designs the optimum C/U_8 is around 250 except for tubular which optimizes at a somewhat lower figure as can be noticed at Fig. 5 and 7, for high and low fabrication cost respectively. The higher effective moderation ratio for the tubular fuel element explains the difference. The optimum C/U_8 is higher for the lower fabrication cost estimate which is quite understandable, since there is less incentive to decrease the amount of graphite which contributes significantly to the fabrication cost.

The influence of the Xenon override requirements can be seen at Fig. 6. Going from 60 to 100 % capability increases the control losses and therefore the fuel cycle cost (higher inventory cost, because of higher critical enrichment, and higher consumption cost, because of lower conversion ratio). The cost increase depends essentially on the rating which is proportional to C/U for a given element and power density. For the full Xenon override requirements, the C/U_8 optimizes therefore at a lower figure as can be seen on the graph for hollow rod and tubular designs at the higher power density. The effect of Xenon override on the teledial and trefoil fuel elements is practically the same as that on tubular fuel elements at equal rating figure.

Effect of burn-up

Since the moderation ratio can be easily adjusted via the heavy metal density in the compact, it would not make sense to envisage the effect of burn-up on fuel cycle cost without acknowledging the fact that a higher burn-up figure puts a lower emphasis on fabrication cost and hence decreases the incentive for low fabrication costs and low C/U_8 . In plotting the fuel cycle costs versus burn-up on Fig. 8 we have therefore optimized C/U_8 for each burn-up figure. It remains of course to be checked whether the heavy metal density corresponding to the optimum C/U_8 is not too high ; but, in any case, a small variation in C/U_8 (and HML) would not entail a significant cost penalty, the optimum being very flat ; besides there is still the possibility of adjusting somewhat the fuel geometry if this were necessary. Fig. 8 shows that even for low fabrication cost, the optimum burn-up figure is still rather high, above 80 MWd/kg and this justifies the development of a fuel element capable of withstanding such burn-ups. Of course, for the higher fabrication cost estimate, the optimum burn-up figure is still higher.

The fuel cycle cost variation for all fuel elements is very similar, except for the hollow rod which optimizes at a slightly lower burn-up figure. The Xenon override requirement has practically no effect on the optimum burn-up figure.

Effect of power density

The effect of power density on the variations of fuel cycle cost with C/U_8 is shown at Fig. 9 for the hollow rod and the tubular fuel elements for two burn-up figures and for 60 % Xenon override requirement. The influence of power density is practically the same for the teledial, when the actual rating is taken into account.

The most obvious influence of the rating is on inventory cost (interest charges on the fuel tied up in the reactor) which decreases when the power density increases. However, a saturation effect develops, due to the increase in critical enrichment, as the amount of reactivity tied up in Xenon override requirement increases with the rating.

The latter effect has also a detrimental influence on the conversion ratio and consequently on consumption charges which tend to increase with increasing power density.

The above effects explain the behaviour of the curves on Fig. 9 : the fuel cycle cost is normally higher for the lower power density but the gap narrows as the C/U_8 and the fuel rating go up. For a low burn-up figure, such as 60 GWd/t, the difference can even be reversed for high C/U_8 because the critical enrichment is lower and consequently, the incentive for high power density less than for 80 GWd/t burn-up.

The curves on Fig. 9 were plotted for 60 % Xenon override requirement and the higher fuel fabrication cost estimate. Full Xenon override requirement and lower fabrication cost would again lower the incentive for high power density.

APPROACH TO EQUILIBRIUM

As mentioned above, the parametric fuel cycle cost studies do not take into account the actual initial cost and the approach to equilibrium which can extend over a long period in reactor where the fuel residence time can be of the order of 5 years at equilibrium.

In the frame of this study, it was not possible to carry an approach to equilibrium study for all the cases considered. We shall only indicate the range of error which is caused by this approximation by giving a numerical example where a detailed approach to equilibrium analysis has been carried out ; the example refers to a case with a long fuel residence time at equilibrium.

In fact, two errors are involved in this approximation, one being made on the fissile material components and the other on the fabrication and reprocessing cost components.

The equilibrium fuel cycle cost normally overestimates the fissile material component because it applies immediately the interest charges on the equilibrium core inventory, which is normally at a much higher enrichment than the first core ; indeed, this first core has no fission product poisoning and is normally designed for a limited burn-up. Also the fissile material consumption is overestimated, because the equilibrium conversion ratio is decreased by fission product poisoning. In the case considered the fissile material component is overestimated by 9.5 %.

On the contrary, the equilibrium fuel cycle cost underestimates the fabrication and reprocessing costs components because it does not account for the partial burn-up of the first and probably also the second core. In this particular case, the equilibrium fuel cycle cost underestimates this component by 25 %. The absolute magnitude of this error depends on that of the fabrication and reprocessing costs.

Through partial compensations of these two kinds of errors, the equilibrium fuel cycle cost underestimates the actual fuel cycle cost by 10 % for the higher fabrication cost estimate and 5.5 % for the lower estimate (for this calculation the plutonium price was kept equal to \$ 8/g in both cases).

GENERATING COST

To be complete, we should finally outline the influence of the power density on the capital cost of the plants. A member company of INTER NUCLEAR is investigating this problem in detail and DRAGON has also made an analysis for hollow rod and teledial fuel elements. The final results being reasonable and coherent, the data utilized by DRAGON for publication will be utilized as a reference ; i.e., the capital cost of the plant increases by 2 to 3 \$/kWe installed when the power density is decreased from 8 to 6 MW/m³ for the teledial and by 4 to 5 \$/kWe when it is decreased from 5 to 4 MW/m³ for the hollow rod.

Taking these data into account, and the influence of power density on fuel cycle cost (Fig. 9), one obtains the total generating cost (Fig. 10) of hollow rod and tubular design. The curves for the hollow rod at 5 MW/m³ take also into account a capital cost penalty due to a power density lower than that of the tubular elements.

COMPARISON OF VARIOUS DESIGNS

Tables 4 and 5 summarize the data on fuel cycle and generating cost issued from this study and taking into account the design limitations outlined in another paper¹. The first conclusion is that all the designs are within a quite narrow band. The relative influence of the fabrication cost and the Xenon override requirement is also very similar. The relative influence of the power density is also quite the same in all designs, except the hollow rod which presents a higher sensitivity to downrating.

Table 4. Relative fuel cycle cost

Fabrication costs		high		low	
% Xe override		60	100	60	100
HR	4	33	34	27	27
	5	32	33	26	26
Tu	6	32	33	26	27
	8	31	32	25	26
Tr	6	33	34	27	28
	8	32	33	27	28
Te	6	32	33	26	27
	8	31	32	25	27

↑
MW/m³

Table 5. Relative generating cost

Fabrication costs		high		low	
% Xe override		60	100	60	100
HR	4	104	105	98	98
	5	102	102	96	96
Tu	6	100	101	95	95
	8	99	100	93	94
Tr	6	102	102	95	96
	8	100	101	94	96
Te	6	100	101	94	95
	8	99	99	93	94

↑
MW/m³

The integral block (B1) is not fully assessed ; its main advantage lays in the reduction of the fabrication cost.

The teledial suffers from the difficulty of concentrating enough fuel into a block ; this leads to a high resonance integral and higher enrichments and hence an age factor preventing to go to the optimum ; the resultant higher sensitivity of the price of enriched uranium is undoubtedly also a disadvantage, in regard to the uncertainty of future evolution of the market. The design could therefore benefit from manufacturing techniques enabling to increase the heavy metal ; some consolidation processes mentioned in a companion paper ² are applicable for this purpose, a further benefit being the reduction of the manufacturing costs.

The main disadvantage of the trefoil is high fabrication cost, relative to the high density fuels.

The tubular presents its optimum at lower moderating ratio's than obtainable by the current fabrication techniques. Further developments leading to fuels with a higher heavy metal loading or burn-up capability would still favour the economic incentive of this fuel.

The hollow rod presents the general advantage of the low rated elements : lower sensitivity to the Xenon override requirements, smaller shift of the optimum moderation ratio with fuel fabrication cost, etc... On the other hand, the generating cost is over 3 % higher than the other elements and the possibilities to improve the economics are small, since the present limits on moderation ratio's and burn-ups are near the optimum for this fuel. Furthermore, the low rating adds the highest running-in period penalty to the costs mentioned in Table 5.

REFERENCES

1. D.J. Merrett and M. Gaube, Choice of fuel design for homogeneous low enriched HTR, Paper presented at the present Symposium (Session V, P.126).
2. L. Aerts et al., Fuel development for a low enriched HTR, Paper presented at the present Symposium (Session V, P.124).

Fig. 1. Reference cases considered.

Fig. 2. Relation between the heavy metal loading and the moderation ratio for the reference cases.

Fig. 3. Resonance integrals for various types of fuels.

Fig. 4. Relation between the effective moderation ratio, the fuel rating and the moderation ratio.

Fig. 5. Relative fuel cycle cost versus C/U_g for higher fabrication cost estimate.

Fig. 6. Influence of the Xe override requirement on the fuel cycle cost.

Fig. 7. Relative fuel cycle cost versus C/U_8 for lower fabrication cost estimate.

Fig. 8. Relative fuel cycle cost versus burn-up (C/U_8 optimised for each burn-up figure).

Fig. 9. Influence of power density on fuel cycle cost for hollow rod and tubular elements.

Fig. 10. Influence of power density of generating cost for hollow rod and tubular elements.

DISCUSSION

R. A. U. Huddle: All the assessments on the teledial concept have been carried out on simple reference design. Now the most important factor in fuel fabrication costs is the amount of graphite work one has to do for a given amount of uranium both fissile and 238. If one takes advantage of this by cutting out some of the unnecessary graphite, this intervening surface for both fuel and coolant, i.e. the so called "frog spawn" modification, then your Nc/Nu ratio is reduced and fabrication costs per unit of fuel are reduced significantly. I believe that, if the fuel cycle costs were reevaluated with such an optimized type of teledial element, then it would show up more favorably than with the simple reference design.

C. Zanantoni: I am not sure I understand your question, however, the fact is that the fuel shape has very little affect on the fuel cycle cost provided the Nc/Nu 238 ratio is the same if the same fabrication costs assumptions are made for the various fuel elements. In my calculations I have assumed that the fabrication cost is made up of two parts, one proportional to the heavy metal loading and one proportional to the quantity of graphite.

H. Kramer: The calculations have been made for an onload fueling I assume. I expect that the cost penalties for Xe overriding which are small due to your calculations when decreasing power from 60 percent will be considerably higher when power is decreased to 40 percent. What certainty will be specified for HTR plants?

C. Zanantoni: I want to point out that your 40 percent power and our 60 percent Xe override are the same thing. In the nomenclature we decided to adopt the possibility to restart after dropping the power from 100 percent to 40 percent, is what we call the 60 percent Xe override requirement.

H. Bairiot: The calculations were done for 60 and 100 percent Xe override requirements. I commented that there was merely no influence of the Xe override requirement on the choice of the optimum (cu/ratio). As could be noticed in the Figure 6, there is indeed a 3 percent cost penalty on the fuel cycle cost. This is discussed more in detail in the paper.

H. Gutmann: You have pointed out relatively pronounced incentive looking at fuel cycle cost only to go from 60 to 80 Mwd per Te discharge indication. I wonder whether you have taken into account the change in thermal efficiency due to higher age factors for longer irradiation?

C. Zanantoni: Yes, we did. I do not think that the discrepancies which may exist between our and your calculations from this point of view can be the cause of such differences. The comments by Dr. Merrett are relevant to the answer to this question .

D. J. Merrett: I would like to comment on the optimization of fuel costs with fuel burnup. The optimization at 80,000 MWd/Te is influenced by the choice of fuel fabrication cost formulae. The formula chosen for this exercise is different from that used in the U.K. studies and arrive at a rather higher optimum than we would expect to find in the U.K. This is the general point that one would expect to find differences between European, U.K. and U.S. market prices for fuel fabrication, thus making direct comparison subject to some difficulty.

C. Zanantoni: We agree completely.

COMPARISON OF HTGR FUEL CYCLES FOR LARGE REACTORS

R. C. Dahlberg

ABSTRACT

Minimum fuel cycle costs consistent with design objectives on power peaking and fast fluence can be achieved in large HTGRs with a fuel cycle defined by a 4 year fuel lifetime, a power density of 8 w/cc, and a C/Th ratio of 250 to 300. This is the basis for current large HTGR design efforts. Recycle of the bred U-233 will be undertaken as soon as the necessary processing facilities are available.

Plutonium is an attractive makeup fissile material in the HTGR. The use of 300 μ fuel particles provides a significant and beneficial self shielding of the Pu-240 resonances, and the resultant indifference value is about \$9.80/gm fissile. Moreover, the fuel cycle cost can benefit from a weakness in the cost of plutonium obtainable on the open market. For example, if plutonium were available at \$8/gm fissile, as might be the case based primarily on a LWR market, then the fuel cycle cost using plutonium makeup fuel in the HTGR would be reduced by 0.11 m/kwh over the reference case with U-235 as the feed fuel.

At the present time, the low enrichment cycle for a fuel element design similar to that developed for the Fort St. Vrain HTGR results in net depletion and fuel inventory costs which are .2 to .3 m/kwh more expensive than in the thorium cycle. Possible lower handling costs associated with the uranium cycle are not expected to change this cost disadvantage significantly. Enrichment requirements are about 12% for the optimum economics, but the fuel cycle cost is not critically sensitive to the enrichment.

THE REFERENCE THORIUM CYCLE

The HTGR concept as developed by Gulf General Atomic is based on the thorium-uranium cycle. Fully enriched uranium is used as the makeup feed material and the U-233 will be recycled as soon as possible. The 40 MW(e) Peach Bottom HTGR utilizes batch loading with a 3 year fuel lifetime. In the much larger 330 MW(e) Fort St. Vrain HTGR, a graded refueling scheme is employed in which one-sixth of the core is refueled each year: the fuel lifetime is six years, and the power density is about 6.3 w/cc.

The fuel cycle for the 1100 MW(e) HTGRs under current design is patterned after that for Fort St. Vrain. The fuel lifetime is reduced to 4 years, however, while the power density is increased to 8 w/cc, somewhat greater than that for Fort St. Vrain. During the recycle mode of operation yearly recycle of all of the bred U-233 is planned.

The power density of about 8 w/cc in the current generation of HTGRs yields fuel temperatures which are near current design limits. Both the fuel cycle cost and the plant costs are reduced with increases in power density. For example, as shown in Figure 1, an increase from 6 w/cc to 8 w/cc for the same fuel lifetime lowers fuel cycle costs by .05 to .1 m/kwh.

The fuel lifetime of 4 years results in a fast fluence to the fuel elements of about 8×10^{21} nvt, near the design exposure. If the power density were 6 w/cc, the fuel lifetime could, of course, be increased without exceeding 8×10^{21} nvt. If a 6 year fuel lifetime were feasible, then the fuel cycle cost would be about the same as for the reference cycle. For a given output, however, the core size would be larger and plant costs would be greater. Thus, total power costs tend to favor the highest practicable power density.

The power peaking "limit" indicated on Figure 1, which defines the region of most favorable power distributions, relates to the age

peaking factor, the difference in local power density caused by the distribution of fuel of different ages in the core. Experience with Fort St. Vrain indicates that an acceptable design can be achieved with age peaking factors of 1.3 to 1.4. Age peaking is adversely affected by both long fuel lifetimes and low thorium loads (or high C/Th ratios). Both lead to lower conversion ratios, larger fractional changes in the fissile loading and thus larger changes in the local power density over the fuel life. A C/Th ratio of about 250 to 275 gives an acceptable power distribution for a fuel lifetime of 4 years.

In summary, a fuel cycle defined by a power density of 8 w/cc and a 4 year fuel lifetime, with C/Th ratios in the range of 250 to 275, yields minimum fuel cycle costs consistent with design objectives on fuel temperatures and fast fluences. A four year lifetime also results in an age distribution in the core which has favorable symmetry, as shown in Figure 2. The four fuel segments, identified by the letters A, B, C, or D, represent fuel of different ages.

A reprocessing policy for the HTGR is shortly expected to be issued by the USAEC. However for foreign applications, in the absence of either an established reprocessing and refabrication industry for HTGR type fuels, or government provision for reprocessing services and U-233 buyback, two variations on the reference cycle have been studied: the storage cycle and the throwaway cycle. In the storage cycle, the discharged fuel is stored in an interim facility until enough fuel has accumulated to warrant the construction of a processing facility. The storage time would, of course, be a function of the number of HTGRs sold, but an 8 year storage time has been assumed for these comparative studies. In the throwaway cycle, the discharged elements are permanently retired with no recycle of the bred fissile material.

A comparison of the reference cycle with these two variations is shown on Table 1 for a 1000 MW(e) HTGR. Delayed recycle would result in a penalty of about .05 m/kwh in the average 12 year fuel cycle cost. The penalty is not larger because after 8 years the accumulated U-233

Figure 1

REFERENCE 1000 MW(e) HTGR FUEL CYCLE

Figure 2

CORE CONFIGURATION; REGION & SEGMENT IDENTIFICATION

would furnish all of the fissile requirements of the HTGR for 3 more years, resulting in good neutronics and a high conversion ratio. The depletion costs are, as a result, low during this interim period. The storage costs, while small, are not negligible. In these comparative calculations, the cumulative costs associated with spent fuel differed between the reference and storage cycles only in the inventory costs associated with the storage cycle.

The cost penalty for a throwaway cycle would be minimized with low metal loadings and long fuel lifetimes. However, power peaking criteria would limit the degree to which this could be carried out although variable orificing as in Fort St. Vrain could be used to permit higher peaking factors than considered in this paper. The cycle shown on Table 1 is typical of the throwaway cycle, yielding a relatively unattractive fuel cycle cost which is about .21 m/kwh greater than for the reference cycle. The spent fuel shipping and disposal costs for the throwaway cycle were assumed to be about \$300/element.

PLUTONIUM UTILIZATION IN HTGRS

In June, 1968, the Edison Electric Institute and Gulf General Atomic initiated a joint effort in evaluating the use of plutonium in HTGRs. The first phase of that study dealt with the value of plutonium as a makeup feed material in HTGRs, and the results of that work indicated an indifference value between \$9 and \$11 per gram fissile (Ref. 1). The relatively high indifference value for plutonium in HTGRs is due to the following factors:

- 1) 93% enriched U-235 fuel at about \$11/gm would be replaced by the plutonium;

Table 1. Variations On U-233 Recycle In Reference Cycle

	Reference Cycle	Delayed Recycle	Throwaway Cycle
Fuel lifetime, years	4	4	4
Power density, w/cc	8	8	8
Fraction of core refueled each year	1/4	1/4	1/4
C/Th ratio	250	250	275
Delay in U-233 recycle (after initial operation), years	3	8	-
Mass flows in equilibrium cycle, kg/yr			
Th-232 loaded	8200	8200 ^a	7400
U-233 recycled	190	- ^a	-
U-235 recycled	20	-	-
Fresh U-235 loaded	340	580	570
U-233 discharged	195	180 ^a	170
U-235 retired	30	45	45
Relative fuel cycle costs, m/kwh			
Fabrication	0.0	0.0	-.02
Reprocessing (and storage)	0.0	0.0	-.14
Depletion	0.0	-.01	+.47
Working capital	0.0	+.06	-.10
Total	0.0	+.05	+.21

^a These data apply to the pseudo equilibrium cycle that exists just before recycle commences. The long term equilibrium cycle is, of course, identical with that of the reference cycle.

Table 2. Relative Fuel Cycle Costs With Plutonium Makeup

		m/kwh Fuel Lifetime, years		
		3	4	5
C/Th Ratio	200	+.23	+.16	+.16
	250	+.12	+.05	+.03
	300	+.06	0.0 (Assumed Reference Case)	-.01
	350	+.03	-.03	-.04

Table 3. Age Peaking Factors With Plutonium Makeup Fuel Lifetime, Years

		Fuel Lifetime, Years		
		3	4	5
C/Th Ratio	200	1.17	1.23	1.27
	250	1.21	1.30	1.39
	300	1.25	1.37	1.47
	350	1.28	1.42	1.55

- 2) the problems in fabricating plutonium bearing elements should be somewhat similar to those expected in fabricating U-233 elements;
- 3) the use of coated fuel particles permits an additional control of the adverse neutronics effects of Pu-240; and
- 4) the benefits of the favorable nuclear properties of U-233 are still obtained.

The plutonium used in these value analyses was typical of that discharged in LWR fuel exposed to an average of 30,000 MWD/MTU, namely:

Pu-239 : 57%
Pu-240 : 26%
Pu-241 : 13%
Pu-242 : 4%

The Pu-240 content of this material is quite high, and unless highly self shielded the absorption resonance at 1.05 ev constitutes an unacceptable neutron poison. The use of coated particles whose kernels range in diameter from 100 to 300 μ provides a significant amount of controllable self shielding. Further shielding occurs in the fuel rods in which the particles are bonded.

As with the thorium cycle, a 4 year fuel lifetime appears attractive. Some comparative data are shown on Table 2 where the relative fuel cycle cost in an equilibrium cycle is shown as a function of both the fuel lifetime and C/Th ratio. 300 μ grains were assumed, but the comparative data are not sensitive to that parameter. The assumed reference cycle, namely a 4 year fuel lifetime with a C/Th ratio of 300, has an acceptable age peaking factor of 1.37. This is shown in Table 3 in a matrix of data similar to that shown in Table 2. Somewhat smaller age peaking factors can be obtained in a 3 year, C/Th=350 cycle at a small increase in the fuel cycle cost. Small reductions in fuel cycle cost can be achieved with fuel lifetimes somewhat longer than 4 years and/or C/Th ratios greater than 300, but the age peaking factor then exceeds the desirable range of 1.3 to 1.4.

Mass flows and other data for cycles of major interest are shown on Table 4. Fissile plutonium requirements for a 4 year fuel lifetime are about 380 kg/yr. Yearly requirements for a 3 year cycle would be somewhat larger. The indifference value for fissile plutonium in the reference cycle is \$9.80/gm fissile. Also shown on Table 4 are data appropriate to a 100 μ diameter Pu particle rather than a 300 μ particle. On the surface, the use of 100 μ particles looks attractive; the indifference value is larger than in the reference cycle, or the fuel cost is lower. However, potential problems exist with end-of-cycle reactivity requirements.

With very small grains and low C/Th ratios a critical end-of-cycle configuration cannot be achieved. For example, it is difficult to achieve a critical loading at end of cycle if the C/Th is 200 and the plutonium grain size is 100 μ in diameter or less. This problem of insufficient reactivity results from the strong parasitic capture of neutrons in Pu-240. As the plutonium fuel loading is increased in an attempt to add reactivity at the end of the refueling interval, the thermal spectrum is hardened so as to further increase neutron capture in the Pu-240 resonance. This problem is much less severe when the 300 micron grain is used to reduce the Pu-240 resonance capture.

Depletion reactivity losses shown on Table 4 do not include the effect of burnable poisons that might be used in a complete design. However Pu-240 acts like a burnable poison since it is converted by neutron capture into the fissile isotope Pu-241. The absorption probability for Pu-240 is much less when the larger 300 micron grain is employed, so this compensating effect is reduced, and the net depletion decrement of reactivity is increased. The increase in required plutonium makeup for the larger grains results from the fact that the fissile plutonium remaining at the end of the reload interval is more shielded in the larger grain. Hence, extra fuel is required to yield a critical configuration at end-of-cycle. In this analysis a 10% increase in the required plutonium feed was observed, and the increase in fuel cycle cost results primarily from this added investment in plutonium.

Table 4. Plutonium Utilization In HTGRs

Bases: Equilibrium Cycle
4 Year Fuel Lifetime
Annual Refueling

Pu particle diameter, μ	300		100
	250	300 (Reference Pu Feed Cycle)	300
C/Th ratio			
Fuel loaded per year, kg			
Th-232	8200	6810	6810
U-233 (recycle)	299	247	239
U-235 (recycle)	33	29	28
Fissile Pu	384	388	352
Fissile Pu discharged per year, kg (retired)	60	44	22
Age peaking factor	1.30	1.37	1.40
ΔK due to depletion over yearly cycle	.042	.048	.033
Relative fuel cycle cost, m/kwh	+.05	0.0	-.04
Indifference value, \$/gm fissile Pu	9.00	9.80	10.40

Table 5. Comparison of Thorium and Uranium Cycles in HTGRs

Bases: Fort St. Vrain Fuel Element
Equilibrium Annual Refueling Cycle

Separative Work: \$26/kg
Ore Cost: \$8/lb U₃O₈
Interest Rate: 10%
Pu Price: \$9.50/gm fissile
Power Level: 1000 MW(e)

	Thorium Cycle	Uranium Cycle
C/Th ratio	250	-
C/U ratio		500
Fuel lifetime, years	4	4
Age peaking factor	1.37	1.48
Relative fuel cost components		
U-235 depletion	0.0	+.04
Pu credit	0.0	-.06
U-233 credit	0.0	+.35
Fuel working capital	0.0	-.07
Total	0.0	+.26

In summary, the 300 μ grain was chosen as the reference Pu fuel to provide extra flexibility in meeting reactivity requirements. The indifference value for fissile plutonium in that cycle is \$9.80. Fuel cycle costs are sensitive to the fissile Pu price; a reduction of \$1/gm in the value of fissile plutonium would result in a reduction of .06 m/kwh in the fuel cycle cost. If fissile plutonium were available at \$8/gm for example, the equilibrium fuel cycle cost for an HTGR utilizing makeup plutonium would be about .11 m/kwh less than the comparable cycle using enriched uranium.

THE LOW ENRICHMENT CYCLE IN THE HTGR

The use of the low enrichment cycle in the HTGR is being continually re-evaluated even though our studies have consistently shown poorer economic performance with the low enrichment cycle than with the thorium cycle (Ref. 2). One advantage of the uranium cycle would be that significant portions of an existing reprocessing industry might be utilized. A head-end facility designed for HTGR type fuel would always, of course, be necessary, and special refabrication facilities would be required if the plutonium were recycled. However, the costs of these facilities would be less than those required to establish the complete technology and facilities for the recycle of U-233. The possibility exists, of course, that the HTGR could be based initially on the uranium cycle with a subsequent change-over to the thorium cycle after many HTGRs were in operation. The necessary base load would then exist to warrant the construction of processing facilities for the thorium cycle. However, the use of interim storage facilities to store the fuel discharged from HTGRs operating on the thorium cycle appears to be a more attractive way of building up the base load.

The thorium cycle in the HTGR has a number of inherent advantages. The fuel in the HTGR is rather homogeneously distributed in the graphite moderator, as shown in Figure 3. The use of thorium, with its relatively

FORT ST. VRAIN
FUEL ELEMENT

Figure 3

Figure 4

LOW ENRICHMENT CYCLE. ANNUAL REFUELING

BASIS: FORT ST. VRAIN FUEL ELEMENT
EQUILIBRIUM CYCLE

low (infinite dilution) resonance integral (about 86b), results in fissile requirements which are only a few percent ($\sim 5\%$) of the heavy metal loading. The use of U-238 with its 277b resonance integral would generally require either a much heavier enrichment or a more heterogeneous lattice configuration than currently exists. Hence, one potential value of the uranium cycle, namely the possibility of using relatively inexpensive low enrichment uranium, is difficult to achieve in an HTGR type reactor.

It has previously been shown (Ref. 2) that for a particular age peaking factor and fuel lifetime, the ratio of fertile-to-fissile absorptions (conversion ratio) and the fissile loading is essentially fixed. The required fertile absorption rate may then be obtained from either a light U-238 loading with a large effective resonance integral or a heavier U-238 loading with a smaller integral. The constant U-235 loading is then purchased at a higher or lower enrichment, respectively. It has been found that over a realistic range of fabrication costs, the expense of fabricating the heavier U-238 loadings (primarily particle costs) essentially cancels the savings due to the reduced enrichment charges.

These general conclusions discussed in Reference 2, have been reaffirmed in more recent calculations reported in Reference 3. These more recent studies of the uranium cycle have assumed in one case the unmodified use of the Fort St. Vrain fuel element, shown in Figure 3, and in another case a fuel element in which a much more heterogeneous configuration could be achieved.

Relative costs assuming a Fort St. Vrain type element are shown on Figure 4. Minimum costs are not associated with minimum enrichments. Low enrichment requires short fuel lifetimes and correspondingly higher handling costs. A four year fuel lifetime results in a design with acceptable fast fluences, and a C/U ratio of about 500 leads to age peaking factors which are a little high but nearly acceptable. The enrichment, however, is about 12%. Longer fuel lifetimes would result in still lower fuel costs, but fast fluences would be greater than 8×10^{21} nvt. A slightly different uranium loading might result in

further small reductions in the fuel cycle cost, but the C/U=500 point is near the minimum. Depletion and inventory costs for this low enrichment cycle are compared with those of the reference thorium cycle on Table 5. The cost of these components in an equilibrium cycle is about .2 to .3 m/kwh greater for the low enrichment cycle than for the reference thorium cycle. This penalty is believed to be much larger than the difference in fabrication and processing costs which under present economic conditions might favor the uranium cycle.

As a limiting case, a fuel element was considered in which a very low resonance integral could be obtained, for example, 30 barns. A fuel element design which would give this result was not developed, but the results of the analysis will indicate the value of potential research and development efforts in that direction. Best results are compared with data obtained with the uranium cycle in the Fort St. Vrain element on Table 6. The enrichment was reduced to about 7% and the depletion and inventory components of the fuel cycle cost were reduced by .08 m/kwh. The total uranium requirements have increased significantly however; in fact, they have doubled. The effect of just the increased particle costs on the fabrication cost would offset the reduction in fuel costs shown on Table 6. Average particle costs, as reported in Ref. 2, are about \$60/kg metal, and for burnups in the range of 75,000 MWD/MT, this extra fabricated uranium would add about .06 m/kwh to the fuel cycle cost.

Thus, low enrichment fuel cycle costs in the HTGR are very insensitive to the enrichment of the uranium utilized. The use of 12% enriched material in a Fort St. Vrain type element is as attractive as the use of 7% enriched uranium in a more heterogeneous configuration. Both, however, are not as economic as the thorium cycle, at least under normally expected conditions.

Table 6. Fuel Cycle Costs for Advanced Low Enrichment Fuel
Element Design

Annual Refueling

	Fort St. Vrain Element	Advanced Fuel Element Design
C/U ratio	500	250
Fuel lifetime, years	4	4
Initial enrichment	.12	.067
Final enrichment	.012	.0048
Pu discharged, kg/yr	44	38
Conversion ratio	.50	.45
Age peaking factor	1.48	1.48
Relative fuel costs, m/kwh		
Uranium depletion cost	0.0	-0.05
Pu credit	0.0	-0.03
Fuel working capital	0.0	0.0
Subtotal	0.0	-.08

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DISCUSSION

R.A.U. Huddle: We had a joint program with H. Bairiot of Belgonuclaire to develop a Plutonium fuel. The fuel was a combination of PuO_2 in carbon black Pu/C ratio 1/20--diameter about 300 micron with a PyC/SiC/PyC coating. Two complete fuel rods were irradiated in Dragon--maximum temperature 1250°C . to a burn up of I think 93 percent. The fuel behavior was excellent and this is undoubtedly our pin-up fuel. Just a word of warning--don't try to make a (plutonium) carbide fuel. There seems to be a volatile competent in this system. The plutonium just vanishes.

D. Tytgat: Could you give some information on the design of your plutonium coated particle?

R. C. Dahlberg: Our studies have assumed pure PuO_2 kernels ranging in diameter from 100 to 300 microns. These can be diluted with Th-232 as necessary to achieve desired fission densities provided the kernel diameter is adjusted to keep the product of the Pu density in the kernel and the radius nearly constant. Coating thickness are expected to be of the order of 150 micron.

D. Tytgat: Within the THTR Association, one irradiation of plutonium coated particles has been performed recently and results will be available shortly from KFA. The capsule contained three different types of kernels, each having the same amount of plutonium but with different diameters.

H. Kramer: How does the temperature coefficient change when using a Pu recycle instead of uranium make up? What are the relative fuel cycle costs of a U/Th and a low enriched cycle for the GGA fuel element design?

R. C. Dahlberg: The temperature coefficient is more negative with Pu feed than with enriched uranium feed. In the U.S., assuming a fuel element geometry of the Fort St. Vrain type, the low enrichment cycle appears to be about 0.2 m per Kwh more expensive than the Th-232/U-235 cycle.

H. Bairiot: As was mentioned by Mr. Huddle, we manufactured at Mol diluted 300 micron Pu oxide kernels; they were submitted to the Dragon project for irradiation experiments: up to 1850°C . (200GWd/t) in the Studvik reactor--up to 600 GWd/t (1200°C .) in the Dragon reactor.

One batch of similar 500 μ kernels and two batches of undiluted kernels were manufactured of coated particles of identical outer diameters also at Mol and irradiated in the Jülich reactor within a joint program with KFA (as part of the THTR project); post-irradiation work will start in a few weeks. Although more demonstration tests are required, the feasibility of such Pu particles is not questionable.

C. B. Zitek: In operating an HTGR is there a parameter such as Bor that has to be met. What parameter is used in determining the acceptable peaking factor line on the draft presented?

R. C. Dahlberg: There is no Bor in HTR. The acceptable peaking factor is based on calculations with due consideration for experimental data on desirable fuel temperatures and fast fluences. We will have thermocouples to determine power output of reloaded fuel.

ECONOMICS OF RECYCLE IN LWR'S AND HTGR'S IN THE U.S.
A CONSULTANT'S POINT OF VIEW

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I. Introduction

Many investigators in recent years have reported on the economics of recycle of fissile inventories bred in thermal converters.^{1,2} In the United States plutonium is seen as LWR recycle fuel in the mid-1970's, and the underlying technology supporting the extension of the uranium cycle to plutonium recycle is steadily developing.^{3,4} Furthermore, its economic implications are under continuous assessment.⁵

Similarly, the neutron economy of the advanced thermal converter favored in the U.S., the HTGR has led to assessments of optimum fuel cycles for this system under specified economic conditions and availability of enrichment plant.^{1,6} In Europe, with the exception of the THTR program, HTR fuel cycle development has in recent years been reoriented towards the slightly-enriched uranium cycle, with somewhat less emphasis at the moment on recycle. In the United States, the thorium cycle has been favored by the American proponent of this system, Gulf General Atomic, Inc.⁷

By the date of commercial recycle of mixed uranium-plutonium fuel in water reactors in the U.S., more than a decade of operating experience will have been obtained in light water fuel technology by utilities, equipment suppliers, and reprocessors alike. As yet, no major U.S. reactor equipment supplier nor independent fuel fabricator has announced a contract with a utility for reload plutonium fuel in a commercial light water plant, even though the inventory value of plutonium in the United States alone will reach approximately \$100 million by 1975 and upwards of one billion dollars by 1980.

Against this background of the relatively lengthy schedule of implementation of recycle in light water reactors, the USAEC and Gulf General Atomic are in general accord on the desirability of recycle in HTGR's at the earliest possible time consistent with the development of recycle technology and a sufficient reprocessing inventory.^{7,8} Studies have been conducted of the optimum time for commencement of recycle of U²³³ from operating HTGR's, and the further development of the technology required to achieve this goal has been reported.⁶

The rationale for continued commercial development of the HTGR in the United States must rest on the willingness of the U.S. utility industry to commit to this advanced system (with appropriate commercial contingencies) concurrently with the continued development required in the fuel cycle. The required utility support during the HTGR development period, we believe, will depend upon the utility industry's assessment of the near-term, relatively assured competitive economic margin of the HTGR, versus competing commercial alternatives, such as light water converters with plutonium recycle, and later, thermal or fast breeders.

Since the water reactor is the immediate competition for the HTGR, in this paper we compare the HTGR thorium fuel cycle with U^{233} recycle and the LWR uranium cycle with plutonium recycle in a self-consistent manner utilizing discounted cash flow evaluation techniques common for this purpose as a basis for comparison. At the heart of the HTGR evaluation are the economic dependences assumed for such parts of the fuel cycle as fabrication, shipping, reprocessing and refabrication, bred U^{233} value, and so on. Selection of these data for the HTGR fuel cycle requires considerable judgment as to the rate of industry growth (throughput as a function of time), projected costs for commercial processing plants, and environmental constraints on plant size as these affect achievable levels of heavy metal throughput per unit of capital invested. Bases for these economic considerations are presented. The view of the utility industry in an as yet undemonstrated fuel cycle is presented as conservative since normal market forces are not as yet operative.

Of equal interest to utilities is the cost structure or economic characterization of the entire fuel cycle, particularly those components that prospectively lie within their control. The fuel cycle cost structure for the LWR and HTGR is examined in terms of differences in the makeup of the direct and indirect fuel costs. In addition, we assess the sensitivities of both fuel cycles to certain variables such as plant capacity factor, value of bred fissile inventory relative to U^{235} , U^{232} penalty effects on refabrication costs, and other variations in the respective fuel cycles. From these analyses the HTGR fabrication cost target for a given reprocessing cost is developed for graphite fuels to achieve parity with current competing light water commercial nuclear fuel offerings, as we view prospective market developments for each system.

II. Economic Assumptions

A. Thorium Cycle

1. Industry Growth

The USAEC have undertaken a systems analysis of the U.S. electric power industry, whereby optimal "mixes" of fossil, hydro, and nuclear plant can be simulated under various conditions of demand and supply in the respective industries (i.e., fossil, nuclear, etc.) and for various economic assumptions (discount rate, amortization period, etc.).^{9,10}

Numerous cases have been examined to investigate the effects of varying parameters on the relative cumulative capacity (growth) of water reactors and HTGRs in an economy with a fast breeder reactor and one without a breeder reactor. The assumptions relate to uranium fuel costs, fossil fuel costs, electrical energy demand, electrical energy costs, and timing of the introduction of the breeder. The analysis is based on a linear programming model used to calculate the minimum cost of supplying U.S. electrical needs for the next half century (1970-2020) for a variety of possible electricity growth patterns. We have taken as our basis for the HTGR (thorium cycle) market penetration the Base or normative Case (B-1) for LWRs, HTGRs and commercial LMFBRs by 1984 with certain modifications. The modification of the indicated growth of an HTGR economy is based on the following NUS assumptions:

- a. U_{308} will continue to be available for several decades at prices of \$7-9 (1970 dollars).
- b. Fossil fuel costs will rise slowly over this same period due to SO_2 removal costs.
- c. A base energy demand of 11.9 years doubling time appears reasonable and consistent with various other projections.
- d. A significant investment (several hundreds of millions of dollars) is required to develop a commercial thorium fuels industry supporting an HTGR economy of the projected size.

The Base Case under the Commission's analyses indicates very little HTGR penetration until 1980-1989, and from the conditions which we believe appropriate above, we have inferred that by 1990 approximately 70,000 MWe of HTGR capacity (cumulative) will be installed, in lieu of 136,000 MWe of MWe of HTGR capacity indicated for the Base Case.¹⁰ We believe this capacity by this date to be reasonably consistent with an initial commercial availability date for an 1100 MWe HTGR in the United States in the 1976-1977 period, and commitments of two or three 1100 MWe HTGRs per year during the early phase of market penetration, leading to 7-8,000 MWe of installed HTGR capacity by 1980. These limits in our opinion constitute an upper bound.

As a lower limit, we would expect slower development and acceptance of this new concept after an initial commitment to an HTGR for startup in

the 1976-1977 time period. Further commitment to the concept by electric utilities would come after 1-2 years of successful Fort St. Vrain operation, say by mid to late 1973, at which time further commercial orders for 1100 MWe HTGRs would be placed for 1979-1980 startup.

These considerations lead us to conclude that growth of HTGRs might be as low as follows:

MWe of Installed HTGR Capacity

<u>Year</u>	<u>Incremental</u>		<u>Cumulative</u>	
	<u>Avg.</u>	<u>Low</u>	<u>Avg.</u>	<u>Low</u>
1976-77	1000	1000	1000	1000
1978	-	-	1000	1000
1979	2000	1000	3000	2000
1980	2500	1000	5500	3000
1981	3000	2000	8500	5000
1982	3000	1000	11500	6000
1983	3000	1000	14500	7000
1984	2000	1000	16500	8000
-	-	-	-	-
-	-	-	-	-
-	-	-	-	-
-	-	-	-	-
1989	2000	1000	26500	13000

From the assumptions supporting the mean, low, and high estimates we project the cumulative capacities of HTGRs presented in Figure 1 and summarized below.

	<u>High</u>	<u>Average</u>	<u>Low</u>
1976-77	1000	1000	1000
1980	8000	6000	3000
1985	~ 20000	15000	9000
1990	~ 70000	25000	13000

For the throughput dependent fuel cycle processes discussed below we have assumed that average capacities are likely to prevail; i.e., 25000 MWe of U.S. HTGR capacity by 1990.

2. Fuel Fabrication and Refabrication

- a. HTGR fuel preparation costs for the assumed industry size and equivalent capacity in tonnes/year of heavy metal (uranium and thorium) are from the literature^{7,11}:

HTGR Fuel Preparation Costs

<u>Year</u>	<u>MWe</u>	<u>MT/yr</u>	<u>Fab.</u> (\$/Kg)	<u>Refab.</u> (\$/Kg)
1975	1000	10	(10)	(25)
1980	6000	65	10	25
1985	15000	160	10	15
1990	25000	265	10	12

- b. HTGR fuel fabrication costs (includes fuel preparation costs and shipment to reactor) are:

<u>Year</u>	<u>MWe</u>	<u>MT/yr</u>	<u>Kg/day</u>	<u>Ref. 11, Appendix D</u>	
				<u>Fab</u> (\$/Kg)	<u>Refab*</u> (\$/Kg)
1975	1000	10	40	360	485
1980	6000	65	240	170	245
1985	15000	160	600	120	160
1990	25000	265	1000	100	135

* Assumes a 30% cost differential on manufacturing costs.

3. Fuel Shipment^{7,11}

- a. Fresh fuel shipment costs to site are:

<u>Year</u>	<u>Fab</u> (\$/Kg)	<u>Refab</u> (\$/Kg)
Before 1980	2.50	8.0
After 1980	2.20	7.0

b. Irradiated fuel shipment costs from site to processing facility are:

<u>Year</u>	<u>Cost (\$/Kg)</u>
1975-1980	17
1980-2000	16

4. Fuel Reprocessing ^{7,11}

Irradiated fuel reprocessing costs after 1982 are:

<u>Year</u>	<u>MWe</u>	<u>MT/yr</u>	<u>Reprocessing Cost*</u> (\$/Kg)
1982	9000	~ 96	~ 120
1985	15000	~ 160	~ 95
1990	25000	~ 265	~ 70
2000	25000	~ 265	~ 70

* From Table 7.8 of Reference 7 at an assumed fixed charge rate of 30%.

5. Bred Fuel Value

The U^{233} value relative to U^{235} (parity value) is based on the re-fabrication penalties shown under 2.b. and an assumed loading of 20 gm U^{233} per kilogram of heavy metal.

<u>Year</u>	<u>Parity Value</u>
Up to 1977	14/12
1977 to 1988 (no recycle)	10/12
1988 on	12/12

6. Thorium Cycle Cost Summary

To recap our choice of the cost assumptions for the thorium cycle with recycle, we have:

<u>Year</u>	<u>MWe</u>	<u>Reprocessing Costs</u> \$/Kg Hi	<u>Spent Fuel Shipment</u> \$/Kg Hi	<u>Fabri-* cation</u> \$/Kg Hi	<u>Refabri-* cation</u> \$/Kg Hi	<u>U²³³ Parity Value</u>
1975	1000	120	17	360	-	-
1980	6000	120	17	170	245	10/12
1985	15000	95	16	120	160	10/12
1990	25000	70	16	100	135	12/12
2000	25000	70	16	100	135	12/12

* From cost data of WASH-1085 (Ref. 7) and NUS-531 (Ref. 11) with an assumed 30% Fixed Charge Rate and nominal throughput of 10 MTH per 1000 MWe of installed capacity per year (lead and lag times have been ignored for simplicity) in dollars per kilogram of heavy metal, initial.

The heavy metal mass balances by batch and year were provided by Gulf General Atomic for an 1100 MWe HTGR. These were found to be reasonably consistent with those reported earlier (Reference 7) for an 1000 MWe HTGR.

B. Uranium Cycle

In developing the costs for the uranium-plutonium cycle in the light water reactor, we have used generally prevailing and accepted market data for ore, conversion, enrichment, fabrication, shipping and processing costs. Judgment is required however in the valuation of bred fuel (plutonium) with

time, and in refabrication costs of urania-plutonia fuels for light water recycle. The bases for our assessment of each is discussed in turn.

1. Bred Fuel Value

The plutonium valuation is based again upon commercial introduction of the fast breeder in 1984, on fabrication price differentials to be discussed, and on plutonium recycle studies in light water reactors by General Electric and Westinghouse under the sponsorship of the Edison Electric Institute.⁵ An independent study¹² has been conducted in which plutonium prices greater than \$7-8/gm (fissile) in the mid to late 1970's (developed in the EEI studies) were obtained. The greater weight of evidence in our opinion is with the \$7-8/gm range, and it is these values that we have used.

2. Fuel Fabrication and Refabrication

The plutonium fabrication penalty is based on the Commission's assumed uranium cycle (LWR) industry size of 150,000 MWe by 1980 and a fabrication penalty derived by BNWL.¹³

We therefore have used these non-market cost data for the uranium cycle:

<u>Year</u>	<u>Uranium -abrication</u> (\$/Kg)	<u>Plutonium Fabrication</u> (\$/Kg)	<u>Plutonium Parity Value</u>
1975	73	100	0.65
1980	54	70	0.75
1985	44	57	0.9
1990	40	52	1.0
2000	40	50	1.0

The heavy metal mass balances were developed by NUS from work sponsored by the Edison Electric Institute referred to above. We chose to analyze a boiling water reactor burning natural uranium feed with plutonium makeup - which the industrial analyst, the General Electric Compnay, found to be the optimal mode of plutonium recycle for their system. This is not a self sustaining cycle in that the amount of plutonium produced during any one cycle is not sufficient to provide enough recycle fuel to form a complete

replacement batch. Four BWRs (or PWRs) provide only enough plutonium for one BWR in this economy. Thus, either more plutonium must be purchased as makeup material for a complete replacement loading, or else only part of a reload is made up of plutonium bearing fuel assemblies. The cost structure and sensitivities given in the following section apply only to the recycle assemblies, so that we have assumed that additional plutonium is available and will be purchased to complete a full replacement loading.

III. Discussion

We have analyzed both the HTGR and LWR fuel costs with recycle using the NUS computer code model, FUELCOST-II. This code calculates the revenue required to amortize entirely each batch of fuel over its lifetime, defined as the time from the first payment for the batch until the final dollar credit is received from the spent fuel material. These "batch costs" are combined in a manner related to when and how rapidly each batch produces energy in order to yield a fuel cost for the reactor. These costs are then discounted to yield a "present worth" or "levelized" fuel cost.

We have incorporated the cost information given in Section II of this paper into our calculations to yield thirty year levelized fuel costs. We further allowed the cost assumptions to change in order to determine the sensitivity of the fuel cost of each system to different assumptions about cost components, as shown in Tables 1 and 2.

The sources of the heavy metal mass balances were mentioned in Section II of this paper. Since they form an important part of the cost analyses, we have tabulated typical equilibrium mass balances for each system in Table 3, although in the economic analysis, detailed treatment by batch was resorted to.

Tables 1 and 2 show the cost structure and cost sensitivities of each fuel cycle as they apply only to these two specific fuel cycles. Other fuel cycles, such as an HTGR without U^{235} makeup (such as Pu^{239} - U^{233} makeup) or a LWR with recycle of only self-produced plutonium, would undoubtedly yield different results. We believe however the case we have selected is the more probable U.S. economy for the near term, based on the General Electric analyses.

For the particular fuel cycles analyzed, we conclude the following:

1. The effect of capacity factor on fuel costs is essentially identical between the two systems. Therefore, neither system shows a cost advantage over the other in areas of the world in which the

future plant capacity factor may be quite uncertain. An example of such a situation is one in which a nuclear plant is being integrated into a predominantly hydroelectric system, and the nuclear plant is not intended to be base-loaded.

2. For equal total fuel costs, the HTGR has higher direct costs, which could have certain advantages in the U.S. accounting treatment of nuclear fuel. For example, a utility might choose to finance a larger portion of the HTGR fuel costs through low cost debt financing than is the case with LWR fuel, while at the same time expanding the rate base.
3. The process costs contribute equally to the fuel cost of each system. One interpretation of this result is that each system can benefit equally from economics of scale and from competition which will develop in the process industries.
4. The uncertainty in LWR fuel costs for a given percentage uncertainty in bred fuel value is several times that for an HTGR.

We should point out that the fuel costs for the LWR economy we have chosen are very dependent on plutonium prices. We have postulated a situation in which the plutonium is produced by other LWR's so that plutonium prices would in fact be related to uranium prices. The LWR would exhibit a sensitivity to U_3O_8 cost as a result through plutonium purchases. We have not shown this effect directly in Table 2, although qualitatively one would expect the resulting sensitivity to ore price to be greater for the LWR than for the HTGR under these circumstances. We have also not considered the likely reoptimization of the respective mass balances for either the HTGR or LWR which would probably ensue from future higher U_3O_8 costs and lower fabrication costs.

IV. Summary and Conclusions

In summary, we have analyzed two specific examples out of many possible modes of recycle. We conclude that the balance between depletion costs, process costs and indirect costs shows that each system displays different cost sensitivities. Depending on specific commercial circumstances, either system might show the lowest overall fuel cost or the least sensitivity to uncertainties in an evaluated result.

As a final point, we have estimated what the process costs must be in order for the HTGR fuel costs to be equal to the LWR fuel costs. The HTGR fabrication cost required for parity with LWR fuel offerings is in the range of 250-300 \$/Kg H (heavy metal) for an assumed reprocessing cost of 70 \$/Kg H (typical of a ~ 25,000 MWe HTGR fuel processing

economy). On the basis of data in this paper on graphite fuel fabrication and processing costs with heavy metal throughput, we conclude that parity with LWR fuel with recycle will be approached with an HTGR industry size of between 10,000 and 15,000 MWe, assuming that prices do not reflect the development costs of the thorium fuel cycle.

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FIGURE 1 ESTIMATES OF CUMULATIVE INSTALLED HTGR CAPACITY
(1984 COMMERCIAL INTRODUCTION OF FBR)

TABLE 1

COST STRUCTURE AT EQUILIBRIUM RECYCLE PERCENT

	HTGR (%)	LWR (%)
<u>DEPLETION COSTS, DIRECT</u>		
U - 235 Depletion	43	6
Pu Depletion		25
U - 233 Depletion	4	
SUBTOTAL	47	31
<u>PROCESS COSTS, DIRECT</u>		
Fabrication	16	13
Spent Fuel Shipping and Reprocessing	5	8
SUBTOTAL	21	21
<u>INDIRECT COSTS</u>	32	48
<u>TOTAL</u>	<u>100</u>	<u>100</u>

TABLE 2
COST SENSITIVITIES IN RECYCLE

A TEN PERCENT UNCERTAINTY IN:	YIELDS THE FOLLOWING PERCENT UNCERTAINTY IN FUEL COSTS:	
	HTGR	LWR
1. \$/lb U_3O_8	3.0	1.0
2. \$/SWU	3.9	—
3. \$/gm of bred fuel value	1.6	6.6
4. \$/Kg recovery cost (shipping plus reprocessing)	0.5	0.6
5. Capacity factor	2.9	2.9
6. Fabrication cost	2.8	1.8
7. Interest rate	3.1	4.6

TABLE 3

ASSUMED EQUILIBRIUM HEAVY METAL MASS BALANCES

	1100 MWe HTGR	1100 MWe LWR*
Metal Loading	9900 Kg	33,600 Kg
U-235 Loading	400 Kg	Natural U
U-233 Loading	210 Kg	—
Fissile Pu Loading	—	1000 Kg
Metal Discharge	9000 Kg	32,500 Kg
U-233 Discharged for Recycle	210 Kg	—
U-235 Discharged for Recycle	30 Kg	0.32 (wgt. %)
Fissile Pu Discharged for Recycle	—	640 Kg
Burnup, MWD/MTU	85,000	27,500

* GEGR — 5566 Plutonium Utilization in Boiling Water Reactors — Phase I Final Report, work preformed for the Edison Electric Institute

DISCUSSION

R. S. Carlsmith: It should be pointed out that the penetration of HTGR's into the U.S. market in the AEC studies (WASH-1126) is not given as a single estimate. Several estimates were given - some of them lower than the one by this speaker. Do your estimates include the effect of a "learning curve" or first-of-a-kind costs for HTGR's in comparison with the established LWR industry?

J. C. Scarborough: We have not included the effect of a learning curve in our projections, since it is not clear to us how in the early years of HTGR development and commercial activity pricing will relate to further manufacturing knowledge. In the LWR industry, the "learning curve" appears to have generally brought higher prices, as Mr. Phillip Sporn has recently brought out.

H. B. Stewart: Your market projection for the HTGR must be based on some limitation such as the introduction date of fast breeder reactors. Can you tell us what you assumed in this respect?

J. C. Scarborough: Our market projection is based on certain modifications of assumptions employed in the USAEC Base Case analysis; however, the assumption of commercial introduction of the LMFBR in 1984 was retained. It is acknowledged, and these same USAEC studies have shown, that a significant increase in cumulative HTGR capacity may occur if the 1984 reference LMFBR commercial introduction date is delayed by several years. We would also expect other commercial arrangements to be required in the United States, such as the licensing of other equipment suppliers, to support the HTGR industry size of our more optimistic range of estimates.

R. C. Dahlberg: I assume from the last slide that the sensitivity of the fuel cycle cost to changes in ore cost is much less for the LWR than the HTGR because it was assumed that plutonium from many LWR's was used to supply the fissile requirements for the LWR under question rather than simply recycling in a reactor the plutonium bred in that reactor. Is that correct?

J. C. Scarborough: Yes, the General Electric study indicated that 3 to 4 BWR's would provide the makeup feed for another comparably sized BWR.

H. B. Stewart: Our calculations, and I think those of others, have

consistently shown that the HTGR fuel cycle cost is much less sensitive to U_3O_8 costs than the LWR. I think it is important that people realize that the U_3O_8 sensitivity you have shown in your table is a highly unusual case where the fuel is predominantly plutonium. Do you wish to comment?

J. C. Scarborough: We acknowledge the fact that the subject of HTGR fuel cycle cost sensitivity has been extensively analyzed and that HTGR fuel cycle cost is much less sensitive to U_3O_8 costs than the LWR without recycle, or with only self-produced recycle. We are not prepared to say that the case we have examined is highly unusual however. Indeed, if the indicated mode of recycle in a BWR is in fact substantially more attractive as the General Electric study suggests, than a reasonable case can be made that this mode would prevail over the near term in the United States.

P.U. Fischer: In your comparison of the economics of recycle in LWR's and HTGR's you have assumed a certain range range for the HTGR market penetration. Since it takes several LWR's to produce the plutonium required for a recycle LWR there will be a limited market for plutonium recycle. Do you have a feeling for the size of this market?

J. C. Scarborough: The plutonium market as recycle fuel for thermal converters, of course, depends on the de facto commercial introduction of fast breeder reactors, or the industry discount of this date. We believe that the annual market for recycle plutonium could range up to 50 MT through 1985, though this assumption is not necessarily consistent with the scheduled LMFBR commercial introduction date of 1984.

H. Kramer: I would like to confirm the comment of Dr. Stewart; due to our calculations for usual full cycles the sensitivity of He HTR is less sensitive to increases in ore prices (by a factor of 3). Which mechanisms are underlying your calculations for predicting the market penetrations of the HTR?

J. C. Scarborough: The bases were those assumed by the USAEC in WASH-1098, Potential Nuclear Power Growth Patterns, as modified by the four assumptions cited in our paper. Chief among these is that low ore prices (less than \$9/lb. unescalated) will prevail until about the 1990 time period. The more recent uranium exploratory drilling results upon

which our judgement on this point is based were not generally available as an interpreted result at the time the analyses reported in WASH-1098 were performed a few years ago. The remainder of the analysis is economic; the technical design bases supporting the respective mass balances for the two systems analyzed have been generally confirmed by other work.

C. Zanantoni: What were the assumptions concerning the plant cost for the two types of reactors?

J. C. Scarborough: Our paper was concerned only with fuel cycle costs as opposed to power generation costs, which include depreciable capital plant and other operating costs. Estimates have been made of HTGR and LWR power generation costs by the USAEC (Reference 7), and an evaluation of commercial tenders which include an HTGR and several light water reactors in the Pacific Northwest of the United States should be available in the summer, 1970.

R. S. Carlsmith: In connection with sensitivity to changes in ore prices it is perhaps worth reemphasizing that the fuel costs for the BWR that was chosen will be very dependent on Pu prices. If one is postulating a situation in which the Pu is being produced by other LWR's; then Pu prices would be closely tied to uranium prices, and the BWR would show an additional sensitivity to U_3O_8 cost through its purchase of Pu. If on the other hand, one wishes to consider a situation in which excess Pu is being produced by LMFBR's; then the Pu price would not rise, and the HTGR's would certainly shift to a Pu-makeup cycle to avoid buying high-cost uranium.

The first of those cases is probably of most interest. ORNL calculations have been made concerning the sensitivity of HTGR and BWR fuel costs to a 10% increase in U_3O_8 cost with the Pu price maintained at a parity of 1.0 to ^{235}U price. Under these assumptions we find 6.6% increase in BWR fuel cost compared to a 2.6% increase in HTGR cost. A 10% increase in separative work cost would cause a 4.2% increase in BWR fuel cost (due to increased Pu value as ^{235}U price increases), compared with a 4.4% increase for the HTGR. These calculations were based on the NUS mass balances and economic assumptions as well as could be determined. Thus they still do not take into account any reoptimization of the fuel cycle.

PARAMETRIC SURVEY ON FUEL CYCLES AND TOTAL GENERATING COSTS FOR
HTR's WITH HOLLOW ROD, TELEDIAL AND TUBULAR INTERACTING FUEL ELEMENTS

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ABSTRACT

Total Generating Costs and Fuel Cycle Costs are given for Low Enriched Homogeneous Design HTR's with Hollow Rod and Teledial prismatic Fuel Elements which are the types considered for the first large HTR stations to be built in Europe. As a more advanced design, the Tubular Interacting concept which requires more extensive proof testing before it can be introduced has been investigated for both low enriched and thorium/highly enriched fuel cycles. The designs are investigated with respect to the manufacturing and design limits at present recommended by Dragon on heavy metal loading, fast neutron dose, surface and fuel temperature limits. The design assessments are made for various power densities on the basis of unirradiated fuel geometries for varying fuel temperature limits. Supplementary studies relate these results to the worst heat transfer condition during irradiation and include systematic and random error assessments. The total electricity generating costs include presently realistic fuel fabrication and capital cost estimates selected according to the principle that only technical and economic considerations determine the choice of contractors.

INTRODUCTION

The task of surveying reactors with the purpose of recommending an optimum reference design or choosing between different design concepts makes it necessary to combine fuel cycle calculations with the corresponding heat transfer and capital cost assessments. Furthermore it is essential for the usefulness of such a survey that it takes into account the material and engineering design limits so that, where necessary, the survey cases can be optimised to the limit

and the optimum cases of each feasibility area can be outlined.

The fuel cycles are, therefore, shown as a constituent part of the interwoven system of physics, heat transfer, engineering, material and cost considerations. The optimum designs are found as minimum total generating cost points within the areas of technical feasibility. It has been made clear how these areas and the optimum designs would be affected by changes in the permissible fast neutron dose, fuel temperature, and achievable heavy metal density.

The paper concerns itself mainly with low enriched fuel cycles for the three prismatic fuel element design types at present under consideration in Europe, that is, Hollow Rod, Teledial and Tubular Interacting. For the latter the low enriched fuel cycles are compared with the corresponding segregated thorium/highly enriched uranium cycles.

FUEL ELEMENT AND REACTOR DESIGN DATA

The representative fuel cells of the three fuel element designs which were investigated are shown in Figs. 1 and 2. (Gaps are not quoted.) The fuel itself consisted of compacts comprising coated particles embedded in a graphite matrix. For the low enriched investigations standard Dragon particles were used throughout the survey. For the thorium/highly enriched uranium cycles breed particles of standard size and feed particles of a smaller size were consolidated into the same compact. The feed particles were diluted with carbon so that equal gas pressure was built up with irradiation in both types of particles. During reprocessing of the compacts the graphite matrix and the outer coating layers were dissolved electrolytically so that the two types of coated particles assumed to be reduced to their silicon carbide layers being of different size could be separated by sieving techniques. The fuel material data and coated particle dimensions are given in Table 1.

The reactor was a homogeneous prismatic design with one stand pipe serving for the charging and discharging of four adjacent fuel

Table 1. Fuel and Fuel Element Design Data for the Homogeneous HTR

Standard Particles (breed particles in case of thorium cycles)		
Coated particle diameter		$\approx 1,100 \mu\text{m}$
Coating thickness		$\approx 150 \mu\text{m}$
Kernel material: Low enriched cycles:		UO_2
Thorium cycles:		ThO_2/UO_2
Feed Particles (only for thorium cycles)		
Coated particle diameter		$\approx 800 \mu\text{m}$
Coating thickness		$\approx 150 \mu\text{m}$
Kernel material		$\text{UO}_2 + \text{carbon}$
Triangular pitch of fuel channel lattice:	Hollow Rod	90 mm
	Teledial	95 mm
	Tubular	95 mm
Number of fuel channels per fuel block		18
Outer diameter of outer cooling channel:	Hollow Rod	63 mm
	Teledial	68 mm
	Tubular	68 mm

Table 2. Reactor Data

Design type	Homogeneous ^a HTR with prismatic fuel elements
Cooling medium	Helium
Moderator material	Graphite
Fuel	Compacted coated particles
Core height/diameter ratio	0.615
Pressure in the primary circuit	56 atm
Axial power form factor	1.2
Radial power form factor (power flattened)	1.2
Direction of cooling flow	Downwards
Cooling of fuel pins: Teledial and Tubular	Internally and Externally
	Hollow Rod Externally
Cooling gas core inlet temperature	305°C
Cooling gas core outlet temperature	785°C
Cooling gas reactor outlet temperature	750°C
Gas mass flow in core	603.8 kg/s
Gas mass flow in reactor	664.6 kg/s
Gross electrical output per reactor	660 MW(e)
Thermal output per reactor	1,535 MW(th)
Electricity generation	Via heat exchangers and steam turbines
Loading scheme	On-load
Xenon override	For a change from 100% to 40% load

^aHomogeneous is to be understood in terms of engineering. The design, however, is heterogeneous from the physics point of view.

element stringers. The reactor data are given in Table 2. The core was contained in a prestressed concrete pressure vessel also housing six pod heat exchangers.

The design limits at present recommended by Dragon are given in Table 3¹. Since the heat conductivities and the dimensions of the fuel compacts and fuel elements change with irradiation the design temperatures had to be viewed with respect to their limits at the worst possible point in time.

SURVEY PHILOSOPHY AND COST ASSUMPTIONS

It is of great interest to know how a variation in the three major design limits, i.e., the ones on the fuel temperature, the fast neutron dose and the heavy metal loading, would effect the optimum design point. Great care has therefore been taken to layout and display the survey with respect to these three variations.

The survey was made in two parts. Part one consisted of the physics calculation with the fuel cycle code MOGA², and the heat transfer and cost calculations with the Dragon design code TECO³. For each design a range of power densities and N_C/N_{HM} ratios was surveyed for unirradiated fuel geometries and heat conductivities under varying nominal fuel temperature limits and for a specific nominal surface temperature limit estimated to correspond to the value given in Table 3. The second part of the survey consisted of heat transfer calculations with DONKEY³ and TIGER⁵ investigating the systematic and random errors as well as the fuel and surface temperature history during irradiation. The final results were then obtained by a combination of the outcome of the two partial surveys.

The fuel cycle calculations took into account an operational margin of 1% reactivity on top of the 40-100% xenon override requirement. They were performed on a 0-dimensional model. The leakage terms, however, represented two enrichment cores with a radius of 0.8 times the core radius dividing the inner and outer core zone. The enrichments yielded from these calculations are therefore

representative of 2-enrichment cores. The age factors were deduced from the time dependent fission cross sections and disadvantage factors.

Since Hollow Rod and Teledial are the designs that can be introduced earliest, we investigated these concepts only with low enriched fuel cycles. In the case of the tubular interacting design, which needs more proof testing before it can be introduced, we studied also segregated thorium cycles.

The low enriched fuel cycles were once-through cases under the assumption that the discharged fuel would be reprocessed and credited.

The thorium fuel cycles considered a segregated concept with U-235 make-up invested only in the breed particles and depleted make-up as well as bred U-233 from the reprocessed breed particles once-through recycled in the feed particles.

All fuel cycle calculations were performed for continuously on-load refuelled systems and assumed a first charge of equilibrium core composition.

In the heat transfer calculations radial power form factor gagging and a continuous age factor gagging for equal gas outlet temperature were applied when needed. All cases were blower power optimised which means that blower power was reduced to the point where either the fuel temperature or the surface temperature reached its limit.

The fully gagged cases where a temperature limit was already reached at full blower power represent a boundary line of the feasibility area.

The cost assumptions are given in Table 4. It was assumed that the fuel and the enrichment service would be bought on the world market. For the assessment of the fuel fabrication and capital costs the nuclear industries in the United Kingdom and Continental Europe have been contacted. Because of the involvement of commercial interests details of these cost assumptions cannot be disclosed.

The contractors were chosen on the basis of technical and economic considerations only. For the fabrication costs it was assumed that 20,000 MW(e) would be installed during the first ten years. The costs were assessed for a situation with several power stations already on order.

It should be mentioned that the fabrication costs for the low enriched fuel cycles, although 2-enrichment cores were considered, assumed only one manufacturing line. This introduced an error of ~ 0.02 mills/kWh which does not however influence the relative comparison between the different designs.

HEAT TRANSFER ASSUMPTIONS AND UNCERTAINTIES ACCOUNTANCY

To arrive at a true comparison between the different cases of the same or different design concepts, we had to work with the actual maximum temperatures. This meant an involvement in studies of irradiation effects as well as both systematic and random errors.

In our investigations with the Dragon design code TECO, we restricted ourselves to the ideal core of unirradiated nominal design specification. The temperatures obtained in this way we call nominal temperatures. In their maximum values they include the radial and axial power form factors and the age factor. It is these temperatures that are referred to in Figs. 7-10.

A separate study, results of which are shown in Fig. 11, yielded the irradiation effect. The temperature values at zero irradiation correspond to the maximum nominal fuel temperatures brought about above. The maximum fuel temperatures occur at about 30% of fuel element dwelling time.

Another study investigated the errors due to imperfections of the actual design. This accounted for fuel eccentricity⁶, coolant leakage, heat leakage, gag adjustments, block and pin power tilts and corrosion. Adding these systematic errors to the above temperature peaks gave the systematic peak temperatures.

A last study covered the random effects. It accounted for the variation of the fuel compact thickness, interface gap width, tube thickness, coolant annulus dimension, fuel enrichment, fuel density, and further for pin displacement, thermocouple and recorder error, control rod effects, etc. These errors on top of the systematic peak temperature yielded the random peak temperatures. It is the latter that have to be compared with the given temperature design limits, that are quoted in Table 3 or the varied fuel temperature limit in Figs. 12-15.

FUEL CYCLES PHYSICS RESULTS

The physics results of the fuel cycle calculations are given in Figs. 3-6. Comparing the results one notices, as could be expected, an increase in the age factor and enrichment with increasing irradiation, while the conversion ratio decreases.

The different carbon/HM-atom ratios (N_C/N_{HM}) were achieved by varying the heavy metal density in the fuel pin. The age factors increase with increasing N_C/N_{HM} values reflecting the decrease of the conversion ratio.

In the range between N_C/N_{HM} 200-400, the conversion ratio of the low enriched cases moves characteristically between 0.5 and 0.65 for both irradiations while the enrichment ranges between 4.5% and 6.5% for 60 GWd/t and 6.0% to 8.0% for 80 GWd/t.

In the thorium/highly enriched fuel cycles one finds higher conversion ratios and consequently better fuel utilisation due to the higher η -value of U-233 compared with Pu-239 and the benefit of the segregated once-through recycle fuel management. The thorium cycles have, on the other hand, the disadvantage of higher age factors since the U-233 fission cross section is smaller than the one of Pu-239.

HOLLOW ROD AND TELEDIAL COST RESULTS

The total generating costs from the survey on the unirradiated fuel geometries are given in Figs. 7 and 8. Each of these figures is actually a superimposition of a manifold of figures because different

nominal fuel temperature limits determine different cost-landscapes, each with its own full-gagging line which limits its feasibility area. Below this boundary is the feasible design area of partial gagging.

All these full-gagging lines for different nominal fuel temperature limits now appear for each design on one and the same figure because we have found that the costs in the regions of interest vary so little with the fuel temperature limit since the surface temperature forces one in all cases to work near full-gagging. We have, therefore, for the sake of a much simpler display, been able to superimpose the various "cost-scapes" without introducing an error greater than 0.003 mills/kWh.

Each feasibility area is determined by the full-gagging line as mentioned above, and either the manufacturing limit of 1 g/cm^3 heavy metal or the fast neutron dose limit of 4×10^{21} EDN which are also given in these graphs. As materials and manufacturing processes develop, the design feasibility area will grow towards higher fuel temperature limits, higher fast neutron doses and higher heavy metal loadings. However, as can be seen from these figures, the cost optima are already included in the present feasibility areas as far as fast neutron dose and heavy metal loading are concerned, so that real cost improvements can be expected only by the easing of the fuel temperature limit. This is especially true for the Hollow Rod design which is apparently much more infringed by this limit than the Teledial design.

All optimum cases occur at full-gagging and turn out with N_c/N_u ratios between 200-300. We expect from previous comparisons of old results with results from the Consortia that these optimum N_c/N_u values are overestimated by approximately 10-20 due to our survey running-in assumption as compared with the outcome of proper running-in studies.

Comparing the Hollow Rod and Teledial design one sees that the latter, due to its additional heat transfer surface in the inner cooling channel, is less limited on the fuel temperature and for that reason allows for much higher power densities, $\approx 9 \text{ MW/m}^3$ as compared to the $< 5 \text{ MW/m}^3$ for the Hollow Rod.

Another significant difference between the Hollow Rod and the Teledial becomes apparent when one studies the temperature behaviour with irradiation as given in Fig. 11. The fuel temperature curve for the Hollow Rod at 4 MW/m^3 and the Teledial at 9 MW/m^3 are roughly the same. Due to the larger dimension of a Hollow Rod fuel annulus compared with one of the eight Teledial Fuel sticks it develops a larger gap between fuel compact and graphite sleeve which results in a higher increase of the fuel temperature up to a maximum with irradiation. The worst point in time is reached after approximately 30% irradiation, and this determines the real design restriction due to the fuel temperature limit.

The optimum total generating costs are obtained by combining the results in Figs. 7 and 8, with those in Fig. 11 including systematic and random errors. They are shown in Fig. 12, while Fig. 13 contains the corresponding fuel cycle costs. The costs are plotted for various fuel temperature limits the core power density being a curve parameter. With increasing fuel temperature limit the cost curves for particular power densities decrease until they reach their total generating cost minimum.

From there on the curves are flat because further increases in the fuel temperature limit cannot be utilised as we have already reached the optimum N_c/N_u value and nothing can be gained in efficiency due to the restriction on the surface temperature.

The envelopes of the individual curves are the real overall optimum results showing how, in the case of more and more stringent fuel temperature limits, it pays to go to lower power densities. We have cut-off our Teledial investigations at a power density of 9 MW/m^3 , as we think that above this power density the thermal shut down stresses across the fuel pin might become too high. The corresponding cut-off for the Hollow Rod, where the heat from the fuel pin only flows in one direction, was chosen at 6 MW/m^3 . This however, would already require fuel temperature limits in excess of 1450°C where we have cut-off our figures.

In the case of the Hollow Rod the cases with 80 GWd/t show no total generating cost advantage over the ones with 60 GWd/t, and for the Teledial, the cost reduction is not very pronounced. Significant, however, is the cost advantage of the Teledial over the Hollow Rod, which becomes more and more pronounced the further one is restricted on the fuel temperature. To be fair, though, we have to admit that the chosen Hollow Rod design cannot claim to be fully optimised. For instance, we have not included a variation of the pitch in this survey. However, we have reason to believe that the chosen Hollow Rod cannot be far from an overall optimised design. In any case it could never close the substantial cost gap to the Teledial design of about 0.2 mills/kWh at the recommended fuel temperature limit of 1350°C.

A comparison of the fuel cycle costs in Fig. 12 with the total generating cost in Fig. 11 shows that the former account for approximately 35% of the total costs.

TUBULAR INTERACTING COST RESULTS FOR LOW ENRICHED AND THORIUM/HIGHLY ENRICHED FUEL CYCLES

These studies did not cover the whole power density range as the previous investigations, but were restricted to 7 and 9 MW/m³. It further has to be mentioned that the thorium results do not have the same degree of accuracy as the low enriched ones. This is due to a less accurate age factor assessment and higher uncertainties in the fuel fabrication costs.

The total generating costs vs. N_C/N_{HM} are given in Figs. 9 and 10. The fuel temperature limit does not put a very stringent condition on this design and it is more the surface temperature that is limiting on the temperature side.

However, since we regard this as a design for the future and because we think that by then the philosophy regarding the surface temperature limit might have changed, we have allowed for excesses of the at present recommended limit of 1050°C by up to $\approx 60^\circ\text{C}$.

The thorium cycle costs in Fig. 10 show a cut-off in the feasible design area which is determined by the manufacturing limit at 35% packing fraction and the assumed restriction to the same build-up of fission product pressure per unit volume void in the kernel as in the corresponding low enriched case. Though this assumption is somewhat of an arbitrary nature, there will be a restriction of a similar kind and in future we simply have to learn more about it.

The optimum total generating costs for various actual fuel temperature limits are given in Fig. 14, for both the low enriched and the thorium cycles, while the corresponding fuel cycle costs are shown in Fig. 15.

Comparing the low enriched Tubular Interacting results of Fig. 14 with the Teledial results of Fig. 12 at the same power densities one finds rather comparable total generating costs. However, it has to be mentioned that while our Teledial fuel pin can be regarded as close to the optimum design, our Tubular design could still be optimised by going to lower pitches and consequently lower power outputs per fuel pin. This is presently under investigation.

COMPARISON OF REFERENCE OPTIMUM CASES

The design and physics data for four reference optimum cases of our survey are compiled in Table 5. Table 6 contains the corresponding cost breakdowns. All reference cases have been chosen for the same discharge irradiation of 60 GWd/t, and the recommended fuel temperature limit of 1350°C.

The comparison of the peak random temperatures in Table 5 clearly shows that the Hollow Rod case is limited on the fuel temperature with a peak random surface temperature well below its recommended limit of 1050°C. In contrast the Teledial and Tubular Interacting cases are limited on the surface temperature, though we have allowed our reference cases to exceed this limit.

The thermal rating varies considerably from the Hollow Rod to the other low enriched cases which is reflected in the xenon override

losses in the neutron balance. The resonances are most shielded in the case of the Hollow Rod resulting in a lower feed enrichment as well as a higher maximum age factor and fifa value.

Due to their similar resonance shielding and equal power density the low enriched Teledial and Tubular Interacting Cases turn out with the same costs, Table 6. In contrast, the Hollow Rod, due to its lower power density and high capital costs is more expensive by ≈ 0.2 mills/kWh.

The thorium case, compared with the low enriched case, has the advantage of a higher fifa and conversion ratio requiring only 2.6 wt.% U-235 make-up per heavy metal loaded, Table 5. It suffers, however, from higher age factors and fabrication costs and gets less fissile discharge credit. Its real restriction, however, is the limitation in the packing fraction which has been discussed in the previous section. Under the present set of assumptions the optimum thorium Tubular Interacting case is 0.04 mills/kWh cheaper than the corresponding low enriched reference case. Learning more about the above limits we feel that we could obtain a thorium reference case with somewhat cheaper costs.

FINAL DISCUSSION AND CONCLUSIONS

All total generating costs minima are found to be within the fast neutron dose and fuel density manufacturing limit at present recommended. The optimum heavy metal density for low enriched fuel cycles is approximately 0.8 g/HM cm^3 for the Hollow Rod design, 0.9 for the Teledial and 0.65 for the Tubular Interacting concept. This shows that the Hollow Rod and Tubular designs are more relaxed with respect to heavy metal loading requirements than the Teledial concept. The running-in assumption of the survey is expected to overestimate the optimum N_c/N_u ratio by approximately 10-20 as compared with a study where the running-in period would have been properly taken into account. This means that such investigations would recommend heavy metal densities that are approximately 0.1 g/cm^3 higher than the ones quoted in this paper, bringing the Teledial up to the present manufacturing limit

while for the Hollow Rod and even more so for the Tubular Interacting design a comfortable margin would still remain.

The Hollow Rod design is the most sensitive to the fuel temperature restriction than the other designs. This prevents one from going to higher power densities and results in a cost penalty of ≈ 0.2 mills/kWh as compared with the Teledial design. Though we cannot regard our Hollow Rod design as fully optimised, we think we are near enough to the optimum to exclude that the very pronounced cost disadvantage could be considerably reduced.

It is obvious from the results that the Hollow Rod design due to its restriction on the fuel temperature side could never be used in conjunction with gas turbines, where we need high gas outlet-inlet temperatures.

The Tubular Interacting design is less restricted on the heavy metal loading side than the Teledial design and less restricted on the fuel temperature side than the Hollow Rod design. In these respects it combines the best of both concepts; with respect to the surface temperature, however, it is more restricted. In the present survey, the Teledial and Tubular optima have effectively the same costs. This is, however, due to the fact that the chosen Tubular design has not been fully optimised.

The segregated thorium fuel cycles, investigated only for the Tubular Interacting design, show, at the optimum, a small total generating advantage of approximately 0.04 mills/kWh over the corresponding low enriched case. This outcome is, however, determined by our feasibility assumptions.

All cost landscapes show flat cost minima vs. heavy metal density, which leaves the designer a comfortable margin for the final choice of the reference point.

For some designs the cost incentive of going from 60 to 80 Gwd/t irradiation is not pronounced. For the first power plants of this kind it therefore seems advisable to choose the lower irradiation

because this would mean a considerable relaxation of the fast neutron dose requirements and would cut down the irradiation required in the fuel and fuel element tests that have to be completed before the concept is accepted.

We have investigated cases with full and partial gagging and have found that the cost optima for all cases investigated occur at full-gagging.

Due to the higher power densities that can be achieved with the Teledial and Tubular Interacting design as compared with the Hollow Rod design, their residence time in days is shorter, which increases the safety margin against corrosion.

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Table 3. Dragon Recommended Design Limits¹

Necessary thickness of outer and inner fuel channel graphite cans (for annular fuel compacts) to guarantee their integrity	5	mm
Manufacturing limit on the lowest possible thickness of the annular fuel compact	7	mm
Manufacturing limit on the lowest possible thickness of the filament between teledial holes	≈3	mm
Achievable coated particle packing fraction	0.35 ^b	
Long-term limit on fuel temperature	1350	°C
Long-term limit on cooling channel surface temperatures	1050	°C
Limit on the fast neutron dose	4 x 10 ²¹	EDN ^a

^aEDN = Effective Dido Nickel Dose (nvt)

^b≈ 1 g_{HM}/cm³ for standard particles

Note: All the given design limits are today's limits and are expected to ease with further fuel development.

Table 4. Cost Assumptions

Cost Normalisation Point: price level on 1st January, 1969		
Item	Dragon Assumptions	
<u>Plant and Capital</u>		
Plant type	Twin Station	
Reactor life	20	y
Load factor	0.75	
Interest rate	8%	y ⁻¹
Tax	0%	y ⁻¹
Insurance	\$ 750,000 per twin station and year	
Inflation	0%	y ⁻¹
Amortisation rate	9.8%	y ⁻¹
<u>Material Costs and Credit</u>		
U ore costs	8	\$/lb U ₃ O ₈
Th cost	17	\$/kg Th
Conversion of U ₃ O ₈ to UF ₆	2.67	\$/kg
Separative work costs	26.0	\$/kg SWU
Tail enrichment	0.2	wt.%
Transport of fresh fuel	2.9	\$/kg _{HM}
Transport of depleted fuel elements	8.0	\$/kg _{HM}
Plutonium credit	10.0	\$/g fissile
<u>Depreciation Factors</u>		
For reprocessed low enriched uranium	0.8	
For twice-through uranium in case of segregated thorium fuel cycles	0.5	
For once-through uranium in case of segregated thorium fuel cycles (Final Charge)	0.7	
Fabrication costs	200-370 \$/kg _{HM}	depending on throughput, enrichment, and fuel element dimensions
Refabrication costs	~ fabrication costs x 1.04	
Reprocessing costs:		
Thorium cycles (including 18 years of storage of irradiated thorium)	40-50 \$/kg _{HM}	} depending on throughput
Low enriched cycles	35-45 \$/kg _{HM}	
<u>Interest Periods</u>		
Fuel ^a	17 (20)	months
Conversion ^a	15 (18)	months
Enrichment service ^a	12 (15)	months
Fuel elements ^a	6 (11)	months
Cooling time ^b	12 (6)	months
Interval between discharge of breed particles and reloading of the bred fuel	21	months

^aThe first value is for replacement fuel, the value in brackets is the first charge fuel.

^bThe first value is for replacement fuel and is an average over the reactor lifetime as determined by the expected conditions for the first HTR power plants. The value in brackets is for the final charge fuel.

Table 5. Design and Physics Data for Reference Optimum Cases

Design	Hollow Rod	Teledial	Tubular Interacting	
Fuel Cycle	Low Enriched	Low Enriched	Low Enriched	Segregated Th/U-235
<u>Design Data</u>				
Discharge Irradiation (GWd/t)	60	60	60	60
Power density (MW/m^3)	4.6	9	9	7
Rating ($\text{MW}/\text{t}_{\text{HM}}$)	46	96	96	62
Residence time (including load factor) (years)	4.8	2.3	2.3	3.5
Plant efficiency	0.418	0.417	0.417	0.417
$N_{\text{C}}/N_{\text{HM}}$	250	280	280	225
Heavy metal density (g/cm^3)	0.78	0.90	0.67	0.815
Packing fraction	0.275	0.31	0.23	0.35
<u>Fuel Temperature</u> ($^{\circ}\text{C}$)				
Peak nominal (fresh)	1026	1062	1080	1065
Corresponding peak during irradiation	1121	1122	1096	1065
Peak systematic	1256	1209	1183	1139
2 σ peak random	1350	1303	1283	1255
<u>Surface Temperature</u> ($^{\circ}\text{C}$)				
Peak nominal	861	898	928	920
Corresponding peak during irradiation	861	923	964	940
Peak systematic	936	1013	1054	1028
2 σ peak random	996	1078	1106	1090
<u>Physics Data</u>				
Fast neutron dose (EDN)	3.0×10^{21}	2.7×10^{21}	2.7×10^{21}	3.3×10^{21}
Max. age factor	1.20	1.16	1.16	1.60
Fifa	1.24	1.10	1.10	1.40
Conversion ratio	0.60	0.59	0.59	0.69
Feed enrichment (wt.%)	5.1	5.7	5.7	-
U-235 make-up per heavy metal loaded (wt.%)	-	-	-	2.6
<u>Neutron Balance</u>				
$\eta \cdot \epsilon$	1.92	1.92	1.92	2.16
Graphite and leakage losses	0.10	0.12	0.12	0.15
Xenon override	0.01	0.02	0.02	0.02
Further control losses	0.02	0.02	0.02	0.02
Other non-fertile losses	0.19	0.17	0.18	0.25
Conversion capture	0.60	0.59	0.58	0.72

Table 6. Cost Breakdown for Reference Optimum Cases

Design		Hollow Rod	Teledial	Tubular Interacting	
Fuel Cycle		Low Enriched	Low Enriched	Low Enriched	Segregated Th/U-235
<u>Capital Costs</u>					
Tender price	million \$	161	152	152	155
Specific tender price	\$/kW	127	119	119	122
Customers on-costs	million \$	33	32	32	32
Specific customers on-costs	\$/kW	26	25	25	25
All-in capital costs	million \$	195	184	184	187
Specific all-in capital costs	\$/kW	152	144	144	147
<u>Fuel Cycle Costs</u>					
Inventory	\$/kW	24	13	13	22
	mills/kWh	0.37	0.20	0.20	0.34
Replacement	\$/kW	77	84	84	69
	mills/kWh	1.14	1.26	1.25	1.03
Total fuel cost	mills/kWh	1.02	1.03	1.03	0.74
Total fabrication costs	mills/kWh	0.62	0.58	0.57	0.59
Total reprocessing costs	mills/kWh	0.07	0.07	0.07	0.08
Total U-credit	mills/kWh	0.07	0.09	0.09	0.04
Total Pu-credit	mills/kWh	0.12	0.13	0.13	0.00
<u>Running Costs</u>					
Interest and Amortisation on capital	mills/kWh	2.26	2.14	2.14	2.19
Fuel cycle costs	mills/kWh	1.51	1.46	1.46	1.37
Tax	mills/kWh	0.00	0.00	0.00	0.00
Insurance	mills/kWh	0.09	0.09	0.09	0.09
Operating cost	mills/kWh	0.32	0.32	0.32	0.32
<u>Total Generating Costs</u>	mills/kWh	4.18	4.01	4.01	3.97
<u>Total Present Worth</u>	\$/kW	280	269	269	266

a. HOLLOW ROD FUEL CHANNEL DESIGN. Δ PITCH = 9.0 cm

ALL MEASUREMENTS ARE GIVEN IN mm.

b. 8 PIN TELEDIAL FUEL CHANNEL. Δ PITCH = 9.5 cm

FIG. 1

ALL MEASUREMENTS ARE GIVEN IN MM.

FIG. 2 TUBULAR FUEL CHANNEL. PITCH = 9.5 CM.

FIG. 3 PHYSICS DATA FOR HOLLOW ROD FUEL ELEMENT.

LEGEND

— PD 6 MW/m³
 — PD 7 MW/m³
 - - PD 9 MW/m³

MAXIMUM AGE FACTOR
 FOR ALL CORE POWER DENSITIES

80 Gwd/t
 60 Gwd/t

LEGEND

— PD 7 MW/m³
 — PD 9 MW/m³

MAXIMUM AGE FACTOR
 FOR ALL CORE POWER DENSITIES

80 Gwd/t
 60 Gwd/t

FIG. 4 PHYSICS DATA FOR TELEDIAL FUEL ELEMENT.

FIG. 5 PHYSICS DATA FOR TUBULAR INT. FUEL ELEMENT WITH L.E. FUEL

FIG. 8 TOTAL GENERATING COSTS FOR TELEDIAL DESIGN VS. N_c/N_{Hu}

FIG. 9 TOTAL GENERATING COST FOR TUBULAR INT. DESIGN WITH L.E. FUEL CYCLES VS. N_c/N_{Hu}

FIG.10 TOTAL GENERATING COSTS FOR THE TUBULAR INTERACTING DESIGN WITH THORIUM/HIGHLY ENRICHED FUEL CYCLES VS. Nc/NHM

FIG.11 MAXIMUM NOMINAL FUEL AND PIN SURFACE TEMPERATURE VS RESIDENCE TIME, $Nc/NHM = 300$, IRRADIATION = 60 Gwd/t .

FIG. 12 OPTIMUM TOTAL GENERATING COSTS VS FUEL TEMPERATURE LIMITS FOR THE HOLLOW ROD AND TELEDIAL DESIGN.

FIG. 13 FUEL CYCLE COSTS CORRESPONDING TO OPTIMUM TOTAL GENERATING COSTS VS. FUEL TEMPERATURE LIMITS FOR THE HOLLOW ROD AND TELEDIAL DESIGNS.

FIG.14 OPTIMUM TOTAL GENERATING COSTS VS. FUEL TEMPERATURE LIMITS FOR THE TUBULAR INTERACTING DESIGN.

FIG.15 FUEL CYCLE COSTS CORRESPONDING TO OPTIMUM TOTAL GENERATING COSTS VS FUEL TEMPERATURE LIMITS FOR THE TUBULAR INTERACTING DESIGN

DISCUSSION

F. P. Ashworth: The total generating costs appear to be insensitive both to tolerable fuel temperature and to burn-up for all these designs. Could you explain these points?

H. Gutmann: Yes, this is true for teledial and tubular interacting fuel elements; for the Hollow Rod it holds only with respect to burn-up. The reasons for the insensitiveness to burn-up is that higher irradiations give higher age factors which subsequently loads to higher gagging, and increase in blower power and a lower plant efficiency. The insensitiveness to the fuel temperature limit can be explained by saying that this is a limit on the peak random temperatures and the gagging have to be adjusted for this. Though going to higher power densities means an advantage in basic capital costs, it also means larger systematic and random errors to be accounted for in an increase in the difference between fuel and gas temperatures. I am glad that you have brought this point up. The results for the teledial and tubular interacting designs show that without losing much in total generating cost, lower fuel temperatures can be chosen which would be beneficial with respect to fission product release.

D. J. Merrett: Your results are very similar to those which were obtained by TNPG, but as pointed out, we consider that the teledial design needs to be downrated to produce acceptable graphite stresses. Would you care to comment on whether you still feel that $9\text{MW}/\text{cm}^3$ is an achievable target.

H. Gutmann: You are referring to the discussions following your paper. I think that the Dragon representatives have made it clear that we do not share your philosophy with respect to shutdown stress limitations. You have said then that TNPG is designing also to 40 percent of the ultimate strength while Dragon have made the point that an appreciably higher value should be acceptable. We, therefore, do not see a need to down rate the teledial design. When asked by E. Smith you stated that if this restriction did not exist the teledial would only be roughly 1 pound per kw more expensive than the tubular interacting design. This is

approximately 0.04 watts per kwh. I agree with you that taking into account differently assumed survey limits there is no discrepancy between our results.

P. U. Fischer: I was surprised to see that lower power density for the thorium as compared to the low enriched case in your last table. We usually find that a higher power density and a fuel loading that gives a lower age peaking factor lead to more favorable power generation costs.

H. Gutmann: Our results show that the tubular interacting design is limited on the surface and not on the fuel temperature. Already for the low enriched cycles we have exceeded the corresponding temperature limit of $9\text{MW}/\text{cm}^3$ by roughly 56°C . The thorium fuel cycles have intrinsically not lower but higher age factors than the low-enriched cycles. Consequently for the chosen thorium reference case at $7\text{MW}/\text{cm}^3$ we still exceed the surface temperature limit by 40°C . I have to stress that we have chosen all our optimum cases taking this design consideration into account and not only on the basis of lower total generating costs. I would like to add that I have made it clear in my lecture that we cannot regard our thorium design as an optimum design, and I feel sure that in the future we would be able to do much better. We just have to learn more about the fuel limitations.

EVENING PROGRAM

Tuesday, April 28

Dinner Speaker - S. E. Beall,
Director, Reactor Division,
Oak Ridge National Laboratory,

"HTGR 's and Greenhouses"

GREENHOUSES FOR HTGR'S?

S. E. Beall, Jr., ORNL

For several years the United States has been undergoing a revolution in agriculture best recognizable in the mechanization of the labor-intensive operations such as planting, cultivation, and harvesting. Not so recognizable, but just as revolutionary, has been the development of the chicken industry - both egg and broiler production. In 1910 it took over five pounds of feed to make a pound of live chicken. You might be surprised to know that at the present time a chicken can be grown for two pounds of feed per each pound of chicken; this has come about through a very vigorous and studious application of agricultural engineering. I believe there are other opportunities in the field of agriculture in which these same principles can be applied.

We are all familiar with the unpleasant feeling of living in a cold climate; it's just a fact of life that it takes some energy to maintain comfort in a cold environment. If sufficient energy is not available to replace that lost to the environment by heat transfer, respiration, evaporation, etc., metabolic functions will be disturbed and the growing process affected. Both warm and cold-blooded animal functions appear to vary with temperatures, as we shall see in a moment. Environmental control of animal and plant enclosures has great prospects for becoming an important factor in the agricultural revolution. We think of environmental control as control of the temperature, humidity, light, soil, and all the things which plants and animals need to grow vigorously, including air. Air has become a very important part of environmental control. A fellow said there was a time when "sex was dirty and air was clean," but that's been reversed now! There are several instances where polluted air has caused the loss of entire crops of tomatoes and carnations. In a controlled environment the air would be cleaned before exposure to plants.

I want to emphasize, though, the importance of temperature control. My first slide shows some data I have accumulated which indicates how important temperature can be in the growth of animals. As you can see, we have plotted percentage of growth at optimum temperature versus

temperature. Note the curve for shrimp, and the steep curve for catfish. Below 20°C catfish hardly exhibit any growth at all, whereas up to 30°C there is a remarkable increase. The same is generally true for milk production, for chicken growth, and also for swine.

Another factor which is important is the efficiency of feed conversion. One has a choice in growing animals of creating the best environmental temperature or letting the feed be converted to produce heat. Using feed as fuel is very expensive, about \$5 a million Btu, compared to \$.50 to \$1.00/million Btu for gas, so one can hardly afford to use feed to keep animals comfortable.

The next slide will show you how the feed conversion efficiency varies with temperature for some of these animals. At 24°C, we have an efficiency of conversion, based on pounds of weight gained per pound of feed consumed of 39% for chickens, whereas at 34°C, the optimum temperature, you see that the efficiency is 52% (1 lb of growth per 1.9 lb feed). For catfish it is even more remarkable. This is an animal that is approaching one pound of live weight for every pound of feed, but 65% conversion is a more normal efficiency. The data for pigs are crude but an individual hog, at 3°C, only converts at an efficiency of 17%, whereas one kept at 15 to 23°C, has a 29% efficiency, a 70% improvement. These data for milk are a little misleading. Efficiency here is based on pounds of liquid milk per pound of dry feed, but note the difference between -13°C and 38°C, a factor of nearly four.

But what one would like to do is to estimate the value that might accrue from controlling the environment. The next slide is an exercise in this fashion. The numbers in column 1 are production rates of food on a per acre basis for uncontrolled environment, taken from several reputable sources. Column 2 lists yields which have been experienced with a controlled environment. You see that 500 pounds of fish per acre in a natural environment is a standard production rate, whereas if one controls the temperature at 32°C and supplies plenty of oxygen and feed, he can produce 200,000 pounds of fish per acre. For broilers an increase of a hundred thousand pounds per acre is possible. For vegetables, tomatoes are the best example: the highest yields in the state of California are

Effect of Temperature on Feed Conversion

	Temp. (°C)	Lbs Feed/Lb Weight Gain	Feed Efficiency (%)
Chickens ¹	24	2.7	39
	30	2.1	48
	34	1.9	52
Catfish ²	20	4.0	25
	29	1.3	75
	32	1.5	65
Hogs ³			
Individual Hog,	3	5.9	17
Group,	3	4.3	23
Group,	8	3.8	27
Group,	15,23	3.4	29
Milk Cows ⁴ (Jersey)	-13	2.1	48
	10	0.65	128
	38	0.55	180

¹Howes, Grub, 1962; Barott and Pringle, 1949²Strawn, West and Dunn, 1965³Sorensen, 1962⁴H. D. Johnson, 1965

Reported Yields (Lbs/Acre)

Food Product	Uncontrolled Environment	Controlled Environment	Estimated Value of Increased Yields
Fish	0.01 × 10 ⁵ (1)	2 × 10 ⁵ (7)	\$ 80,000 acre/year
Eggs	7 × 10 ⁵ (2)	8 × 10 ⁵ (2)	\$ 30,000 acre/year
Broilers	5-7 × 10 ⁵ (3)	7-10 × 10 ⁵ (3)	\$ 40,000 acre/year
Beef	8 × 10 ⁵ (4)	Not available	Not available
Pork	7.5 × 10 ⁵ (5)	8.3 × 10 ⁵ (5)	\$ 20,000 acre/year
Vegetables, e.g., Tomatoes	0.6 × 10 ⁵ lbs/acre (6) (One Crop)	7.5 × 10 ⁵ (8) (Three Crops)	\$100,000 acre/year

1. Nations Agriculture, March 19702. Poultry Science, Vol. 37, No. 5, 1958

3. J. R. Howes, W. R. Grub, Auburn University Experiment Station, 1962

4. Calculated from information by J. B. Billard, National Geographic, February 1970

5. J. Mentzer, Purdue University Experimental Station

6. USDA Yearbook, 1968, California Yields

7. W. C. Yee, personal communication

8. Heysler and Ruthner, 1968, and M. Bentley, Commercial Hydroponics, 1959

about 60,000 pounds an acre for open field cultivation. Now in a controlled environment such as a greenhouse where one can control water, fertilizer, CO₂, temperature, etc., three crops a year are possible and total production could reach nearly a million pounds per acre per year. In fact several of these foods can be produced today at a rate of a million pounds per acre per year, and others can reach this level with the proper application of available technology. In the last column I have estimated the value of the increased production that comes with controlled environment. My estimate varies from \$20,000 an acre for pork to \$100,000 for an acre for tomatoes.

Now my next slide* is a picture of a greenhouse full of tomato plants which average about twenty pounds to the plant and which can produce in the neighborhood of 600,000 or 700,000 pounds per acre on a three crop per year basis. The next slide* shows cucumbers grown in great proliferation, under carefully controlled conditions. An interesting point here -- in the first century it is recorded by Pliny the Elder in his Natural History that Emperor Tiberius loved cucumbers. He ordered a cart covered with isinglass for his gardener to grow cucumbers in the off season. During the daytime the cart was rolled out for the beneficial effects of the sun, and at night the cart was rolled back into the house. Two thousand years ago people were growing cucumbers in greenhouses!

It appears that all we need to accomplish year-round growth and higher yields of lots of foods is cheap heat, and now, a question for the audience: "Where on earth could we find the source of cheap heat in large quantities which might also solve some heat pollution problems?" A few months ago Don Trauger and I were out at Fort St. Vrain, Colorado, and there we saw a large fertile area of land being devoted to a nuclear reactor. This site is some 2200 acres and is very prime agricultural land. Fort St. Vrain, as you know from all that you've heard at this meeting, is the High Temperature Gas-Cooled Reactor which Colorado Public Service Company is building. 330 Mw(electrical) will be the operating level and at 40% efficiency it will produce 500 Mw of thermal energy at 102°F, to

*Color photo not included in this paper

be thrown away by means of cooling towers. With free land and free heat, how can we miss? We (Garland Samuels and I) propose that the cooling towers of this reactor, which would cost about a million to a million and half dollars could be essentially replaced with "greenhouse cooling towers." We think that the greenhouses could be heated in wintertime and cooled in summertime with a device called an evaporative cooler. The next slide is a picture of such a cooler. It's a very cheap affair made from galvanized wire about 2 in. thick stuffed with aspen fibers or Spanish moss. The way it operates is that water, in our case warm water (102°F) flows into the top of the pad while a large flow of air is pulled horizontally through the pad. Both the air and water temperatures in the fiber approach the prevailing wetbulb temperature. Thus one can maintain desirable temperatures in the summertime, and this is especially true at Denver, Colorado, where the wetbulb temperature runs in the neighborhood of 65°F. It is fairly easy to show that one can maintain a summertime temperature below 75 degrees in the greenhouse by evaporating enough water to cool the water and the air. The complete system is shown on the next slide to give you a better picture of how it works. There is an evaporative pad at one end and two fans at the other end. The fans draw at fairly large rates, like 150,000 lb/hr, through the pad while 40,000 to 80,000 lb of water/hr flows down through the pad. In the summertime the air is blown to the outside, which is the way a cooling tower works, but in the wintertime when you want to maintain a warm house, then one has to conserve the heat in the air by recirculating. We have installed a plastic sheet, to form an attic, to allow the recirculation of the air back through the warm water coming from the reactor station. It is clear to us that one can control temperatures, let's say, at 75 to 80°F in the summertime and above 65°F in the wintertime. In case high humidities are expected to be a problem, we have provided an option of a row of finned tubes downstream of the pads so that some dry heat can be added to keep the humidity in the 70 to 80% range. The remarkable thing about the system is that one can beat the existing cooling towers at Fort St. Vrain by about 13°F; that is, he can send colder water back to the condenser with this system than he could with the cooling towers; this is important because it means a greater efficiency of conversion to electricity in the normally warm summertime.

Another very important aspect of this heating-cooling system is the cleaning of the air by the air-water contacting. As mentioned earlier, it appears likely that crops cannot be grown near cities in the future unless the dirty air is cleaned by some process. Water washing is an extra benefit of the evaporative pads.

Since Fort St. Vrain is near Denver, it is natural that we would look at Denver for an estimate of the fresh vegetables and poultry which the city uses, so we can determine how many greenhouses and poultry houses are needed. The U.S.D.A. records of shipments to the city of Denver for the year 1968 show a total unloading of out-of-state produce of 113 million pounds of fresh vegetables. I am not including potatoes or grains. Even at two cents per pound, the cost of shipping a hundred and thirteen million pounds is about a million dollars a year that could be saved. Also, assuming that Coloradians eat chickens as frequently as other people in the United States, they could use 20 million broilers in the environs of Denver, and again one can calculate nearly another million dollars for shipping because few broilers are grown in Colorado. So here is an opportunity for the entrepreneur who is willing to make use of the Fort St. Vrain heat to produce these foods. On the basis of these shipments one can calculate that it would take 500 acres of greenhouses and maybe 50 acres of poultry houses to supply the city of Denver. Fort St. Vrain can't supply quite that much heat, but it can supply enough heat for 200 acres. Actually, we can show that 100 acres would be sufficient to dispose of all the 500 Mw heat.

One or two hundred acres at \$175,000 per acre for greenhouses is not an exorbitant investment compared to the investment of the power plant itself. One does have to look carefully, though, at the capital cost, and I know you wonder whether or not such a thing would pay off. Greenhouse operators in the state of Colorado expect to spend \$.50 to \$1 per square foot for their heating equipment. This comes to \$25,000 to \$40,000 per acre, which is a sizeable investment. They expect to spend from \$4,000 to \$8,000 per acre in heat bills to keep their crops growing at the maximum rate. From my contacts in the business, I am certain they are making a profit even with these large heat costs. What about our system?

First, for capital cost, the additional equipment needed to deliver the heat from the reactor would be pumps, piping, electrical equipment, an emergency heat of several hundred megawatts, all estimated to cost about \$28,000 an acre (for 100 acres). Although we would like to take credit for replacing the cooling tower, I doubt if Colorado Public Service would agree to that. If they did, we would get a credit of about \$10,000 per acre and reduce our cost to \$18,000 per acre; if they didn't the cost for heat supply would be \$28,000 per acre, which compares with the standard installation cost for greenhouse heat systems.

Now consider the \$4,000 to \$8,000 per year normal heat bill. If we can heat 200 acres of greenhouses, with free warm water, the profit for somebody would be in the neighborhood of a million dollars a year. I don't propose that all of it go to Colorado Public Service. I imagine they would be happy to share it with the greenhouse operator to entice him to build near them in an arrangement such as shown on the last slide. Colorado Public Service could possibly be ridding itself of a capital expense for the cooling towers that would be a couple of hundred thousand dollars per year at 14% annual charge, and an operating expense which I think might be \$50,000 a year.

So on paper, at least, and certainly that's not the last word, this system looks feasible and profitable to the grower and the utility. I think it can be applied to other agricultural operations besides greenhouses and, of course, any power plant could be used as the heat source. The title of this talk as listed in your brochure is "Greenhouses for HTGR's?". I would like to remove the question mark, and those who want to invest in the Fort St. Vrain Greenhouse Association meet me on the right after this talk!

Fig. 1. Effect of Temperature on Growth or Production of Food Animals.

Fig. 2. Pad Assembly.

Fig. 3. Greenhouse and Air Flow System.

Fig. 4. Exclusion Area Boundary.

PANEL DISCUSSION:
PROSPECTS FOR THE NEXT TEN YEARS

J. A. Lane, Moderator	Oak Ridge National Laboratory
C. L. Rickard	Gulf General Atomic
R. D. Vaughan	The Nuclear Power Group, Ltd.
K. Wirtz	Kernforschungszentrum Karlsruhe

PANEL DISCUSSION

PROSPECTS FOR THE NEXT TEN YEARS

J. A. Lane, Moderator, ORNL

When Mr. Trauger was putting this panel together and asked me to be moderator, I am sure he did so with the knowledge that I am fond of making projections about the future of atomic energy. He probably assumed that in that capacity I would contribute some such projection for gas-cooled reactors to the discussion. Unfortunately, the projections I make are not in the same ball park as that being considered by this panel. My projections are very long range, are based on very meager information, and are aimed far enough in the future that they cannot be verified while we are all still around. In contrast, the question before this panel concerns the near-term prospects for gas-cooled reactors, is based on real facts of life information, and is much more difficult to answer. Any such projection we make today, moreover, can or cannot be verified in the relatively near future. In other words, we have to be careful about what we say! Fortunately, we have three experts on this panel who are ideally qualified to give you some insight into the short-term outlook for gas-cooled reactors, and I will leave it up to them to do this.

Before turning the session over to them, however, I have one comment to make about the ultimate role of gas-cooled reactors; namely, that the long-range outlook appears to be quite sound. The various studies and evaluations of nuclear power systems that have been carried out during the last several years, particularly those that were done for the United States Civilian Power Program, all show that gas-cooled fast breeders can contribute significantly to the overall economics of the systems. Their long-term role can be either an alternate or a partner to liquid metal-cooled fast breeders. Because of their economic merit, therefore, gas-cooled breeders should in the long run become an important segment of various nuclear power systems throughout the world.

The nearer term role of high temperature gas-cooled converter reactors, however, is less certain because two possible roles exist. On the one hand, HTR's or HTGR's might be developed merely as a stepping stone to the longer range gas-cooled breeders. This role would be relatively unimportant in terms of total system megawatts. On the other hand, a second possible role for HTGR's exists; namely, that they might become the main embodiment of advanced converter reactors and introduced as a means of saving on power costs and at the same time conserving resources of low cost uranium. The same U.S. Civilian Power Studies mentioned previously showed that the introduction of advanced converters during the next decade or so could save the U.S. power system from \$2 to \$8 billion depending on the introduction dates and performance of subsequent fast breeders. These are not actual dollars, but actual dollar savings discounted at 7% back to 1970. Thus there is a sufficient economic base for introducing thermal gas-cooled reactors into the nuclear power system. To make their maximum impact, however, they must be built as soon as possible and in large numbers. In the system analysis studies mentioned, more than one hundred 1000 Mwe HTGR's were assumed to be added to the U.S. power system during the period 1976 to 1990. Thus the near-term role of gas-cooled reactors does not depend upon technical feasibility or economics, but upon the ability of current equipment suppliers to capture a market for and supply so many reactors in such a short period of time.

Our next panelist, Mr. Corwin Rickard of Gulf General Atomic, should have a lot to say on this problem of marketing gas-cooled reactors because that is his company's main business right now.

Corwin Rickard, GGA

1. It has often been said that gas cooling offers an open ended potential for improvement in reactor performance. The reports at this conference together with the last ten year's experience with gas-cooled reactors has certainly provided a dramatic illustration of this.

We have seen:

- a. Plant efficiency go from about 25% for the early Magnox reactors to 40% for HTGR's
 - b. Core power density increase by more than a factor of ten from less than 1 kw/liter to over 8 kw/liter with a similar increase in power density for steam generators, and
 - c. Fuel utilization improved by more than a factor of two such that less than half as much uranium need be mined to start up and operate an HTGR plant for 30 years, i.e., about 2000 tons U_3O_8 for 1000 Mwe. What will the next ten years bring?
2. First, let's look at the market. The forecasts indicate that about 190,000 Mw's of capacity in about 150 units will be ordered between now and 1980 in the U.S. alone. A substantial part of these can be gas cooled. Add to these in the U.S. the units that could be gas cooled in Europe, Japan, and elsewhere around the world, and it is easy to come up with a minimum estimate of 60 units. It could easily be twice this number, representing over \$25 billion in gas-cooled reactor plant orders.
3. The first units can be expected to have characteristics like those, for example, in the paper by A. J. Goodjohn. Overall plant capital costs should be less than for LWR's.
- a. Waste heat is about 35% less than for LWR's
 - b. Throttle steam flow is about one-half
 - c. The steam turbine is smaller and costs \$9 to \$10 million less than a LWR turbine in 1000 Mw sizes
 - d. Liquid and gaseous radioactive wastes are small and economically controlled to low levels
 - e. Pressure times the volume of the primary circuit, and the stored energy in the coolant are both less than for PWR's, thus, both primary and secondary containments should be easier.

There are a number of other factors, as well, that lead to the expectation that plant costs will be less than for LWR's. Improvements will come along as more of the units are sold.

- a. Core and steam generator power density will increase by 10-15% without significant design change
- b. Plant sizes will increase at least to 1500 Mw.

4. Supplier Industry

Unlike some other reactor coolant technologies, gas cooling has a ready made supplier industry with worldwide and redundant capability for HTGR-PCRV construction, steam generator fabrication, gas circulation equipment, and other HTGR components. The similarity of HTGR equipment with conventional power plant equipment should greatly ease requirements for industry investment in new fabrication equipment and should help hold costs down as component business increases.

5. Fuel and Fuel Cycle Characteristics

All of the HTGR fuel element designs look good from the performance reported here. We seem to have block elements, ball elements, and pin elements, but from the fabricators point of view, these are not any different than differences say between PWR and BWR elements. They all require about the same in fabrication equipment. The next ten years will surely see a proliferation in HTGR fuel manufacturing plants.

It would appear that carbide kernels, oxide kernels, low enriched, high enriched, thorium, Biso, and Triso fuels will all be used to meet differing needs which certainly points to the advantage of flexibility of HTGR fuel.

At the same time, the papers here indicate that performance of the fuel will be further improved toward higher allowable temperatures permitting higher power density cores, higher ratings, and reduced fuel inventory. This, plus increased throughput in fabrication plants, will result in a marked decrease in fuel cycle costs.

^{233}U recycle following the excellent work here at ORNL should start during the next ten years. The March 1970 announcement by the U.S. AEC to extend buyback of ^{233}U and guarantee HTGR fuel reprocessing provided needed encouragement for the pursuit of the U-Th cycle

in the U.S which promises substantial improvement in both uranium utilization and fuel costs.

6. Pu-Th Cycle

The paper by Dahlberg reporting on EEI studies of Pu utilization in HTGR's indicates they can make good use of LWR generated Pu. In the absence of breeders, the next ten years should see some HTGR's benefiting from LWR Pu or some LWR's benefiting from HTGR's making good use of their plutonium.

7. HTGR Existence with Breeders

It is often pointed out that eventually breeders will help LWR's by buying their Pu. Breeders can also help HTGR's by supplying them with ^{233}U . The first looks at this are just being taken, but it could lead to a further reduction in HTGR fuel consumption and inventory of about 25%. For this and other reasons, perhaps HTGR's and breeders will happily live together.

8. One got the impression from this conference that the HTGR steam cycle development was over and done with and now the R & D was turning to gas turbine cycles. I believe this impression will be verified during the coming ten years. The planned program in Germany is certainly to be congratulated, as well as the good work in the U.K. and Switzerland. The papers brought out the great potential of gas turbines-HTGR plants.

- a. Capital cost reduction is expected because of the direct cycle and physically smaller turbines. We know high temperature steam turbines are smaller and cost less than low temperature saturated steam turbines. Gas turbines, owing to the high working pressure (they are not trying to get work out of a vacuum the way steam turbines do) are vastly smaller still, and therefore, should cost less.
- b. Easier and more economical rejection of heat through air coolers may prove essential in places where supply of cooling water is exhausted.

9. Process Heat

The conference, and in particular, the excellent paper by Krämer indicated the increasing interest and need for high temperature process heat HTGR's. The last ten years saw the building and startup of the 2500°F UTREX-HTGR. The next ten years will no doubt bring a great deal more development in this area, including the new programs being discussed in Germany and Japan.

GCFR

The Gas-Cooled Fast Breeder Reactor papers have not been presented yet, but there are some excellent papers to be given in this area tomorrow afternoon. Compared to other gas-cooled reactors, the great attraction of the GCFR is the potential it holds for a very large improvement in fuel utilization. For example, while a 1000 Mw HTGR will require about 2000 tons of U_3O_8 over 30 years, a 1000 Mw GCFR would produce the fissile equivalent of about 1500 tons U_3O_8 which is certainly a worthy goal to be sought by gas-cooled reactor development.

Perhaps more important at the present time, however, is the opportunity the GCFR provides for a parallel and alternative development to the LMFBR.

Commissioner Johnson has written in Power Engineering that he would, ... "like to see the GCFR and the LMFBR brought along together in development."

Philip Sporn stated before the JCAE, "The gas-cooled breeder reactor promises so much in the way of higher specific breeding ratios and lower costs that national prudence almost demands a continuation of work on this alternative...."

Ralph Davis, Chairman of the 41 member utility group supporting GCFR development has said, "The U.S. national breeder program would be strengthened by including the GCFR. This would provide competition of concepts and technologies and increase probability of developing a breeder attractive to the utility industry."

I believe, therefore, that we can look forward to increasing activity in GCFR development in the years ahead.

R. D. Vaughan, TNPG

Dr. Rickard has dealt so thoroughly with the material we have been discussing at this meeting and with the American situation that I think it falls to me to fill out the situation in Europe and to bring out some of the differences in approach which have been adopted over there. I would also like to dwell a little more on some of the immediate problems we are encountering in the actual design and construction of gas-cooled systems.

May I deal first with the short-term situation, that is the next five years. In the United Kingdom there are eight reactors of AGR type now under construction and four more are shortly to be committed by the CEBG. The first units of this programme of reactors will be going into commission next year. They are of integral design with concrete pressure vessels and will have single channel access for on-load refueling.

The first high temperature reactor of commercial size will be ordered by the CEBG in mid 1971, to be on power late in 1975. It will be a single 600 MW unit at Oldbury 'B' and the contract will follow competitive tenders from the two Design and Construction companies now active in the U.K. In fact, the CEBG, to secure the best possible design, has already placed design contracts with both of us, and it is from ideas being generated under these contracts that a formal enquiry specification will be prepared later this year.

The CEBG are then expected to revert to their custom of building twin reactor stations and the pace at which the HTR programme proceeds will depend very much on our technical progress in certain specific areas. Irradiation tests on the fuel must show satisfactory behaviour under typical reactor conditions. We must also demonstrate that on-load refuelling is a practical proposition as it has been on the magnox reactors. The CEBG are very anxious to know that the fuel and the reactor as a whole is capable of load following, and they are also pressing for a design in which there is good vessel access so that all high temperature items can be inspected after operation. It is expected that there will be a substantial cost differential with respect to the AGR and this will be an important factor in determining how quickly HTR is introduced.

On the possibility of the gas-turbine HTR, proposals have already

been prepared for the construction of a prototype unit to follow a year or two after the first steam raising HTR. Current estimates of the saving offered by the gas-turbine system are fairly modest and amount to 7 percent on capital cost and 5 percent on unit generation cost. We do not see any justification for using the gas-turbine cycle to raise the thermal efficiency above the 42 percent now readily obtainable with the steam cycle. The main doubt about the gas-turbine HTR, from the point of view of an electric utility, is the availability that can be expected of it. In an integral design where most of the components are tucked away inside a concrete pressure vessel, the ease with which they can be removed and replaced will be critical.

The development work involved in a gas-turbine project is quite extensive and such a scheme is unlikely to go ahead except as a collaborative project involving two or more countries in Europe.

On the continent of Europe the first commercial HTR will probably be a 300 MW pebble bed design, developed from the AVR project. I would expect and hope the second HTR would be a 600 MW unit with prismatic fuel along the lines adopted for Oldbury 'B'. The scale of the programme, which I expect to be based on Germany, will then depend on the attractions of the high temperature reactor in relation to light water systems.

The high temperature reactor must firstly have lower costs, currently estimated to be 8 percent on capital and 6 percent on generation. The heat rejection rate, of course, will be much lower, about two-thirds that of the light water reactors. This I understand is becoming an increasingly important factor in the U.S., and will also apply in Europe.

The high temperature reactor has a lower usage of fissile atoms than all other systems except the CANDU reactor and the fast reactors. I feel I must also differ with Mr. Scarborough in his remarks this morning on this point. According to my calculations, the high temperature reactor uses about two-thirds the quantity of ^{235}U required by the light water reactor, and it is accordingly less sensitive to future increases in ore costs. Likewise, the effect of a reduction in value of plutonium in the spent fuel of a high temperature reactor is about half what it would be for a light water reactor.

Now let us deal with the longer term, where I am sure there will be

an expanding market for the high temperature reactor if it maintains its promise on costs.

On the technical side it is not difficult to make some reasonable projections into the future. In three or four years we shall be developing extruded fuel pins, with or without a fuel free layer on the outside. This, of course, would not apply to the type of fuel brick which GGA have developed for their reactor. But fabrication costs will benefit considerably as experience is gained and throughput increases.

Gas temperatures today are in the region of 750°C . and I should think for the steam raising HTR these could rise to 800°C . and level off at that value. I can see no reason to depart from the sub-critical steam cycle with temperatures, as today, at 538°C . There is no doubt that the station output can be doubled without any technical difficulty, and in fact, you are already proposing this in the United States.

With increases in gas pressure from 60 bar to 80 bar, I would expect we shall raise the core power density from 8kW/litre to 10kW/litre, but these pressures and power ratings will, I think, be limiting. At the same time, of course, the fuel rating will be increased from 80 MW/Te to 100 MW/Te. I think the incentive to raise thermal efficiency will not be very great and it will remain at figures which are current today.

The gas-turbine variation of the high temperature reactor I can see will have an integral design at 80 bar and temperatures ranging from 850°C . to 900°C . Again the incentives, in my view, to go much beyond this temperature are not very great. The development content of the rotating components of a gas-turbine high temperature reactor is so high that they will have to be designed and manufactured on a quantity basis, as in the aircraft industry. For this reason one or, at the most, two standard compressor turbine designs will have to be adopted. I think a unit of 300 MW would serve very well and these could be associated with power turbine units of 600 MW to remain reasonably within the technical limitations of materials and test facilities.

There is one further system of which we have said little at the moment. This is the gas-cooled fast reactor, for which I think gas pressures of 120 bar will be justified. At such pressures a core power density of 300kW/litre will be obtainable and gas temperatures should

be about 800°C. as for the steam raising HTR.

On the commercial side, I think the situation in Europe is clearly different from that in the United States. We have quite a number of groups working in the field at the moment and there will be a need for more collaboration, particularly on fuel design, both within the UK and through Europe. The irradiation facilities in fuel designs is so high that we must clearly make every effort to evolve one common fuel design which can be employed in different reactor configurations.

I also believe there is not room for more than two design groupings in Europe. There must be two in order to stimulate competition, but if there are too many the technical effort will be spread too thinly and the reactor designs will suffer. Collaboration is relatively easy in the study phase when only small sums of money are being spent. A very good example is the GBR Association in Brussels, where excellent communications have been established with all design and experimental workers in their field across many national borders. But this is a fairly loose association. Permanent groupings of the various organizations in the field, capable of spending substantial sums of money, will only come clear when one or two specific power stations have to be committed.

Contrary to Dr. Rickard speaking on the U.S. situation, I think that in Europe there is a need for more collaboration on fuel manufacture. Whereas some competition may be required here, I think there are five groups at present in Europe, and they clearly cannot all compete economically for the relatively small number of orders there will be in the next three or four years.

I believe that in five years' time orders for high temperature reactors will begin to be placed as routine in preference to other systems and it will then be the turn of the fast reactors to challenge the position they have established. I think successful work on the gas-cooled fast reactor could result in the start on construction of a prototype reactor providing, of course, governments can find money for the project. This is a fairly buoyant picture, but one which will only come about if some calculated risks are taken, so that earlier projects get under way and experience is gained as quickly as possible.

Karl Wirtz, Karlsruhe

It is generally agreed upon that for an ultimate success of nuclear power production breeding will be necessary. It may be obtained by two types of breeder reactors - thermal breeders and fast breeders. So far, no economic thermal power reactor is able to breed, and no proposal has been made for an economic thermal power breeder reactor. It is not impossible that advanced concepts like the Molten Salt type finally will prove feasible for economic thermal breeding. There is no doubt that economic thermal breeding would be a major breakthrough and would have considerable advantages compared to fast breeders.

Many studies have shown that fast breeders have the potential to become economically competitive with present-day and future power converters, for instance, with the LWR and HTGR types.

The major effort on FBR's all over the world is concentrated on sodium cooling, mainly because at the time when these programs were started sodium was the appropriate choice.

The following facts have changed this situation in recent years:

1. The development of helium-cooled reactor technology for HTGR's
2. The use of prestressed concrete vessels for gas-cooled reactors
3. The fact that large and economic resources of helium became available in the natural gas sources, for instance in Europe.
4. The possibility to use in a first generation the same basic pin-type fuel for both types of fast reactors under the same temperature conditions.

Further aspects of gas cooling for FBR's point to a future second generation. Their potential is higher temperatures, the possible use of gas turbines, and last but not least, the high thermodynamical efficiency with lower percentages of waste heat combined with the possibility of cooling against air instead of water, and thus avoiding the heat pollution problems of rivers on many inland sites.

The GCFBR based on present-day fuel pin development presents primarily problems of correct design of the primary high-pressure circuit.

There are many arguments that technically it poses problems that are not of a principal character and should be solved by straightforward designs. All safety problems can be analyzed adequately. The sodium-cooled FBR, on the other hand, until today poses principal design problems connected with the intimate interaction of the coolant with the neutronic behavior of the core.

Recent evaluations seem to indicate that even at equal surface temperatures of the oxide fuel pin, and with corresponding steam temperatures, the GCFBR offers the same or even lower costs per kilowatt hour than the sodium-cooled FBR, mainly because of lower capital costs.

These favorable aspects of gas-cooling of FBR's have stimulated several study programs in Europe as well as in the United States. These studies are supported by government laboratories, manufacturers and utilities, but so far the money involved is only a small percentage of the amount spent for sodium-cooled FBR's. On the other hand, the percentage would be misleading with respect to the results of these studies, because, as we mentioned before, the GCFBR presently can rely heavily on the two major efforts, the HTGR and NaCFBR. This underlines that it is appropriate to consider gas cooling as just one of the competitive cooling possibilities to attain the common goal - fast breeder reactors.

What has to be done in coming years? The prospects of GCFBR's certainly justify the present modest research programs that will hopefully be increased in coming years. Examples for such programs are:

1. Certain heat transfer studies as are going on, for instance, in Karlsruhe
2. The development of higher temperature cladding materials, for instance, of the Vanadium alloy type and of the advanced stainless steel types
3. The development of coated particles for high fast fluences and the research on promising cermets
4. The research on prestressed concrete vessel designs
5. Carbide fuel development that is of equal interest to NaCFBR
6. Studies on active and passive engineered safeguards, etc.

Moreover, in the coming years there probably will be the strong desire to include a gas-cooled fast breeder reactor prototype power plant in the present development programs. As we have seen, this is well justified. In view of the fact that at least five to six major nations are considering the construction of a prototype sodium-cooled FBR, it should not be impossible to invest some money to further the later arrived competitor who seems to be equally promising. International cooperation would seem to be particularly appropriate. It would make economic sense for everyone, as it would ensure production of information necessary to enable both systems to be judged on their economic and safety merits. It would also comply with the responsibilities of the nuclear community concerning the ultimate goal - a safe and economic FBR. This could be accomplished best by developing both coolants. It would also create competition between the manufacturers and give the utilities a chance to select the type they liked best.

FLOOR DISCUSSION

H. Krämer: I would like to make some very general remarks because all the technical and financial details we got by this excellent presentation by our panelists, and I would like to add some very general remarks. We all followed this meeting, and we got a review on the work which has been done in the different organizations on the HTR field. I think we all got the impression that the HTR is now ready from the view of the development work; that now we can envision the phase of commercializing the HTR system. I think we all agree with this now, but what has to be done for entering this commercial pace? I think, there are two main decisions. One is that the utilities will engage with this system and also that the utilities would make decisions. The utilities will have to trust in the HTR, and we hope that will be done in the next years. The utilities, they have to say that they will intend to spend the money which is necessary to make the work on industrial scale for further developing all the details, all the components which are necessary when introducing into the commercial pace, and we all know that this will cost quite a lot of money. Now what is the situation?

I would like to make another statement for, that is, we all believe that the HTR has very good presumptions to be introduced in the energy market because of several reasons. We know that the technical problems are at least in principle solved, and they are less than in other reactor systems, I would say. The economics are quite well for the HTR system. The safety is all right so far as we know today. The sum of pollution is better than in other systems, and we also hope that availability is quite well with this system, because as we have already heard, the accessibility is quite good for this system, the primary circuit is quite pure, at least as we know it today. There is also a big development potential, and we have heard about the HTR steam cycle, direct cycle, gas-cooled fast breeder, and there is process heat, and if I may go further, there is also fusion. We all know that helium technology will help also in this field. You see there is a very straight line concerning the future development potential. Now I will come back to these decisions

which will have to be made on the side of the utilities and the companies. In the United States, the companies ... have made a clear decision as far as I learned here. They will spend the money which is necessary to introduce this system, and what we hope that also the utilities in this country will trust in the system and make the right decisions. In the United Kingdom, I am rather sure that the decisions will also be made within the next year. In this country, I think things are much easier than in the United States, and especially in Germany, because there, there is a long tradition in gas-cooled systems. There is a lot of experience in construction of these kind of reactors, and therefore, we can be hopeful that within the next year the decisions which I mentioned will be made in England. In Germany, I think we are not yet as far. The work is still not set by our companies, and we have also to convince a bit more of our utilities. We will do our best, of course, but I think that an international cooperation, which you all mentioned, would help things very much and I hope that what you said can be realized in the next year, and if I may make a prospect for the next year, it is then this: That I think there is a big probability that within the next year there will be a decision whether we will overcome the barrier to the commercial phase or we will not.

P. Fortesque: First of all I would like to say how much we welcome Professor Wirtz's remarks. I think a few more voices like that would do us a lot of good. What I really rose to say was, I wanted to raise one rather broad point about the gas-cooled fast reactor which I think tends to be a little obscured. At meetings like this, one hears a great deal about projected mills per kilowatt hour, conversion ratios, and heaven knows what for the year 1990. Now all of this kind of stuff is far from the whole ingredient. I think if that were true we would all have nothing but homogeneous aqueous reactors today. I think a very important feature determining whether vast sums of money are going to be expended by utilities on huge plants, a very necessary ingredient, will be the record of those plants looking back at them. Now, in order to form such an opinion you must have a record of more than one type of plant to form that opinion upon, and the point I wish to make is that too much preoccupation, excellent though a little bit is, too much preoccupation with mills per

kilowatt hour can be obscure. One very good point and reason for advocating the pursuing of two different breeders is that only by pursuing two different breeders will industry be able to accumulate the statistics necessary to really judge how long those breeders will remain on line, and irrespective of what the ultimate answer is, a most important degree of freedom is taken from the users if he is only presented with one choice to make; you have it or you don't. There is one other slightly related point I would like to make, I think, while I have got (the microphone). I noticed Jimmy Lane remarked in the opening that, I think he used the phrase, "HTGR is a stepping stone to the GCFFR." Well, in context that is fine, but perhaps that is open to a little misrepresentation if that is taken to mean that we only regard the HTGR as a stepping stone for fast reactors. Far from the case; in fact, the HTGR is just as much, and even more, benefited by the coexistence of breeders as the light water reactor. The light water reactors have the benefit of a sink for their garbage, the Plutonium. The HTGR doesn't just provide a sink for garbage, it benefits vastly from the fact that the breeder program, be it gas cooled or sodium, could in principle produce Uranium 233 in its blankets, and to benefit to the HTGR from use and excess. The 233 is far more than the benefit of a garbage disposal for Plutonium and the reason goes far beyond just the physics. We all know about the physics of the better fuel 233. What receives far less attention is the better heating transfer properties. Now that is an extraordinary statement, but what I mean is that this the fuel volume occupancy requirements of U^{233} -fueled HTGR are much lower than those of a 238 fuel, leaving a much more area for the cool passage, and you can get very much better ratings indeed with 233; so we regard the two projects not as rivals for 1990 model, but as coexistent, helping, symbiotic, whatever it is you call them.

B. Chapman: We have had a lot of papers in this meeting on optimization, but all of them, I think, have been concerned with steady state. Now, one of the things I think everybody agrees on is a general increase in unit size, and what that brings with it is the problem of the fast load pickup when you lose one station off the grid. Now I am a fairly committed HTR man myself, but I believe in the philosophy of "know thine

enemy," and I think the saturated water systems do have perhaps an easier route to this fast load pickup problem. I wonder if the panel can comment on whether they think that this sort of consideration would seriously influence the course of development to the HTR.

R. Vaughan: I think I could answer that with a question. What particular feature of the HTGR is it you feel may hamper its ability to pick up load and to better other systems?

B. Chapman: Well, I think at first sight one might feel the reserve provided in the system by a large amount of saturated water, and the ability to pick up load by simply dropping pressure would on the face appear to be an advantage for the wet steam system. If you intend to do this with the HTGR, you have got to pick it up pretty well straight from the core itself, and this would have quite sharp implications on fuel design, I would think.

C. Rickard: Yes, I just might say a thing here. I think the design that we are offering, and it is also the case with the Colorado PSC, is designed to be able to pick up about 5% per minute load variation and that is the normal requirement of the systems, and we haven't in discussions with the utilities have not gotten a feed back, a large advantage in excess of that; and the 5% per minute, it seems to be about normal with conventional plants and theirs is probably limited by turbine temperature changes, and so we are with the HTGR, we are fitting into a normal pattern that has been established previously throughout the country, so I don't think that will have an limiting effect on HTGR plant application.

R. Vaughan: It is true we have made life a little more difficult for ourselves than it used to be on the Magnox stations by using once-through boiler systems. When we had natural circulation boilers, we had a reservoir of the type you were talking about, and we have let that go in order to get the economies in integral design. So your point is well taken, I think.

C. Zitek: What has Peach Bottom's experience been in load pickup ability?

C. Rickard: I think that Peach Bottom, when it's operating, has been basically base loaded, but it is a plant to follow load, but as far as I know Peach Bottom is capable of at least a 3% per minute load change and it's been able to do that.

P. Fischer: I had several discussions in Europe on this topic. As far as Peach Bottom is concerned I think one has to distinguish between the rate which may be a programmed rate increase, and what kind of step changes it can take, and I think Peach Bottom has gone through some rather rough step changes on the system of the Philadelphia Electric Company. It's too bad John Kemper isn't here, I think he could give the details on that. I think, generally, that Peach Bottom can pick up rather reasonable step changes in loads without any big disturbances to the temperature, pressure, and key parameters. I think as far as supporting water reactors is concerned, I think, the interest in Europe is to change somewhat faster than here, but I think the point is a sales point that has been maybe a little overstated; these 2% per second that are sometimes quoted for this systems are over a limited range. It's more compared to a step change, almost, at that rate over say, 30% load. I think this is not what the normal grid will require. I think it is designed, it could probably be designed, to the customer requirements. I think, as Rickard said here, I think it is 3 to 5%. It might be interesting to hear whether in the United States, from the utilities side, the increase that is expected.

C. Zitek: I still think the limitation is on the turbine generator unit. Water reactors will pick up load as fast as you want really. We have done this. I have another point I want to bring in. I am fairly new in this gas cooled bit. I am led to believe the waste disposal problem is much less in a gas-cooled than in a water-cooled. Still, I have not heard that this is an advantage in the gas-cooled. With the big human cry that we have about radioactive discharge in the environment, here in the States at least, it would be definitely to your advantage to design a plant that has essentially zero activity release to the environment. I have been pushing for this in the LFMBR's, and the companies indicate that yes, they could probably do this. Have the gas-cooled people thought of doing this as a selling point?

C. Rickard: Yes, I will answer that one. It certainly is an advantage in the sense that to get very much lower level release, it doesn't cost as much to get them bottled up and then shipped off-site so that you can turn that advantage into real dollars in plant construction, and we have in the case, we have now offered an option going with that that provides for a waste gas system and a liquid waste system, so that the discharge from the plant to the environment around us is very, very low, very, very low. Much, much lower than current discharge levels.

A. Goodjohn: The gas-cooled reactor, of course, doesn't generate a lot of solid and liquid waste. Most of our wastes are in the form of gases, and I want to comment first of all on the fission products. The gaseous fission products are removed by a means of a purification system. I denoted that in my talk on Monday. This purification system takes about 10% of the primary coolant per hour through it and then returns the purified helium to the PCRV. That purification system removes the dust, and removes the gaseous diffusion product by a means of ... cold trapping, and removes the hydrogen and tritium by means of a titanium sponge. Now these products normally are removed by means of a gas waste system. That is, when we have to regenerate that purification system. Normally we put the products from our helium purification system into a gas waste tank, and let it go to the atmosphere under controlled meteorological conditions. Now in our option that we are providing here, we are essentially taking the material from our purification system. Speaking now specifically of our long-lived fission products, specifically Krypton 85, we are taking it and putting it right back into the PCRV. With that 10% of the primary coolant per hour going through the purification system, the Krypton 85 will simply build up and be retained in the low temperature traps. There will be no release of Krypton 85 except as made by the normal leakage of containment helium from the PCRV. We expect that would be very, very small, very, very small. Insofar as tritium is concerned - again now, when we regenerate that temperature trap, normally we would put it in the gas waste tank and put it up the stack under controlled meteorological conditions. With our zero-pollution option, we will take that trap and regenerate it through an oxidizer and make tritiated water.

That is one way to do it. It then becomes a very concentrated liquid waste - tritiated water, we, in fact, do not even have to do that. That tritium sponge is replaceable simply by taking it out and replacing it with a new sponge. So that, in fact, we do not have to release any tritium to atmosphere from our purification system. There will be a small leak of tritium probably, again from a small leakage in the PCRV, but we expect that would be very small.

C. Zitek: Are you going to be able to measure leakage from the containment?

A. Goodjohn: That is always very hard to do. We designed our containment to be subatmospheric, slightly, by just a few inches of water during normal operations. I do not expect that we will get very much leakage from the containment.

M. Dalle Donne: I would like to make more of a comment rather than a question. I think that the high temperature thermal reactor would have the introduction of the gas-cooled breeder in respect to the sodium cool breeder for the following reasons: It is often said that the main advantage of the sodium breeder in respect to the gas-cooled breeder is the smaller design inventory. It seems rather difficult with the gas-cooled breeder to have a plutonium inventory as small as that one which is possible with sodium. This smaller inventory would help the introduction of a sodium breeder because with a limited amount of plutonium coming from the light water reactor, for instance, it is possible to make more reactors with smaller inventories; but if the high temperature thermal reactor is one entering into the market very strongly, then the amount of plutonium available for the breeder, of course, will be less and the introduction of fast reactors will be dependent upon the doubling time rather than the inventories. In this case I think the gas-cooled breeder will have an advantage in respect to the sodium breeder, because I think that it is quite likely that the doubling time for the gas breeder would be better than for the sodium breeder.

J. Lane: Actually in connection with the specific inventory our studies show that the gas-cooled breeder had the same specific inventory

as the reference LFMBR, but was a factor of 50 to 80% higher than the advanced systems; but there is an advanced gas system that also has a low inventory. The difference isn't that great, in other words.

D. Tytgat: I have some comments which may be complementing, but first of all I think we all agree speaking from the European community, about what has been said by Dr. Krämer about the future of the HTR. We conclude that this is a reactor which has a high potential, and we certainly welcome future international collaboration in all fields. We could even comment that we are always speaking initially with collaboration in Europe, but at Euratom we have excellent collaboration with USAEC. Previously we could say even that maybe we introduced competition by having an extensive water-cooled reactor exchange of information, which I think had a great impact on the whole European situation. I have some small comments on some of the points raised during the meeting; certainly for the matter of prestressed concrete pressure vessel which is one of the key new developments in the U.S., I agree with the words of other colleagues. We certainly believe in these pressure vessels in Europe, by the number of vessels which have been built. Maybe the philosophy of the United States for the moment is to use secondary containment. Maybe the European philosophy might be more to take great care about the installation, about which we heard from Mr. Vaughan, which might be one way to really be sure that a prestressed concrete pressure vessel has really full security. Another point is concerning the fuel. I had the impression there might have been in the introduction some confusion due to the different presentation in the fuel session. That means some presentations were more on fuel specification tests, while some other papers concentrate on fuel developments. Let us say at Euratom we certainly believe completely in the excellent behavior of this coated-particle fuel, and certainly we completely agree on the Dragon side too; and we have been quite impressed by the work done in the United States on the coated-particle matrix, and the graphite irradiation results. As was pointed out, there may be still a small gap to be filled, but it will be filled very soon about this sort of large-block irradiation, but we have full confidence that they will confirm all the particle calculations up to now. I have one point to mention maybe on the guarantee.

It was much in the discussion about this guarantee given by Nukem for the first 300 megawatt THTR. While maybe for some of the American representatives, this may be having some relevance or meaning, but I should say that we have been working for a very long time with Nukem, and we know that if they are ready to give a guarantee, that they really believe and feel it's perfect and will give all satisfaction to the electricity producer. There are other points on technology. Take AVR, which was a very special concept, and the fact the AVR is working perfectly and going to (a 300 megawatt version) proves that even if some particular problems were introduced by the HTR, they have all been solved in a very short time, as also mentioned by the panel; and we certainly agree that this proves that even if there are some component developments to be made, as Dr Krämer said, all those problems will be solved.

R. Huddle: Could I just bring to your notice a modern version of an old Dragon project proverb. One experimental result is worth a thousand computer calculations.

J. Bugl: While we have been sitting here now together for three days in a rather pure environment as we are used to. What I want to say is that we are sitting together among the family of high temperature reactor people. Now to get through to develop such a system, of course, it needs quite an amount of public money and it needs, of course, also the customer, so is it still permitted to ask, say a representative of the USAEC and perhaps a representative of a customer of a utility utilities to say a few words on what impression they had on our meeting.

R. Pahler: First of all I am more identified with the HTGR, I think, than the AEC; so I would like to say that the program we are sponsoring on gas-cooled reactors, we are spending \$3,000,000 in 1971 on our base program, mainly oriented for fuel development. We are spending \$2,000,000 on formulization which is our recycled program. Now this is not an announcement, but I want to make a statement which Corwin (Rickard) referred to on our U²³³ buyback policy statement was issued. Will it extend the guaranteed purchase price for U²³³? This is a public release issued about a month ago, and this is the Commission speaking and not Pahler: "In addition to guaranteeing the purchase price of U²³³, the

AEC conducts a research and development program which provides support for the high temperature gas-cooled reactor concept. The AEC budget request for the fiscal year 1971 provides for continuation of its development efforts on this reactor system. Recognizing its promise of efficient fuel utilization, high temperature efficiency, and improved environmental conditions over existing reactors (and I make that statement specifically back in the corner with respect to water reactors), the AEC plans to continue support for the HTGR beyond fiscal '71, within the limitations, of course, of the availability of funds. Through 1977 the AEC will accept and make financial settlement for HTGR fuels if commercial reprocessing is not available. Consideration will also be given to the need for a further extension of this policy at a later date." Now this last statement refers to a published price that we are coming out with, for reprocessing HTGR fuels similar to what was done for light-water reactors. I don't know whether it is in the public record yet, but it is soon to be announced publicly.

L. Graham: I don't want to finish on a facetious note, but I wonder whether the panel would state what the probability of a U.S. reactor being sold in the United Kingdom or in Europe is in the next 10 years and vice versa.

C. Rickard: I think that there is certainly always that possibility. I think that the whole HTGR program is moving forward in a spirit of cooperation and in the Fort St. Vrain reactor there are a number of components which have come from Europe and we think with possibly the building of HTGR in Europe that might reciprocate a bit sometime in the future, but I think that the main job at hand is to get the first big one started.

R. Vaughan: Agreed. No reactor is built in another country. It is built in the country on which the ground where it rises from the ground. Some features of its designers, as Rickard has just said, may come from somewhere else, but still the reactor is always indigenous at the end of the day.

K. Wirtz: Let me remind you on the past in Germany, there have been built water reactors more or less by American companies, not directly,

but in a way that they are in license agreement with the German country, and I think this will be the way in the future too, and I guess it is a good chance it will go both ways.

K. Wirtz: I have one question which may not be possible to answer yet. The panel has discussed the relative merits of helium turbine direct cycle plant compared to a steam cycle. You have mentioned that some international cooperation may be necessary to build the first direct cycle in the plant. What is your feeling to the size of this plant? You have to have something like an experimental demonstration of such feasibility of this direct cycle helium plant; and how small can it be in order to be still big enough to allow to draw conclusions which are of some value later on larger stations?

R. Vaughan: I think it must for several reasons be a fairly large plant. First of all, you will remember I made the point that the rotating components have to be developed for mass production. Perhaps that is a strong word, but you know what I mean. Therefore, the first components you make must have the dimensions and characteristics of those components you will want for your later stations. So if you are going to want 600 or 1200 megawatt units, we will say 300 megawatts to the compressor units in them. That is the size of the units you want to test and manufacture. There is no point in making one which is one-third the size because you could not justify the development work economically on the unit because you would have to do it again for the large unit. Also the other purpose of your prototypal, the value of it is to test the control feature, and these will vary widely if you go from say a small unit with two pods to a large one with four pods. You have got to reproduce as closely as possible the dimensions and the characteristics of your ultimate power stations so I think it must be a real prototype; a prototype in output as well as in temperatures and pressures.

K. Wirtz: Still let me say that I am not sure that with just one plant we can get all the information we may need, especially with respect to the control question. It could be that if anyone would go really into the problems of the control question that it could turn out that you could get this information through a fairly small plant, say of the plant

we have envisaged in Germany; of course, this doesn't hold for the turbines and so on. But my feeling is that so far the question with respect to control, that means the interactions of the whole circuit, haven't been analyzed adequately so far, at least not to my knowledge, I must say. I would very much like to have some kind of information about whether for this purpose a big plant would be necessary or not. It could well be that a fairly small plant, say 20 to 50 megawatts, would be adequate.

C. Rickard: Yes, I think that I would like to add, there is certainly something to say on that argument on the multiplicity of a plant and possibly small ones as well as large ones. We certainly learned a great, great deal from our experience with the 40 megawatt Peach Bottom plant, and we are learning a great deal more with the 330 megawatt Fort St. Vrain plant and both of them had real value; so I think it would be hard to say there is only one approach to getting on with the job of combining gas turbines with HTGR's and I think the main thing is to get moving ahead on that job because there is very interesting potential in the future there for that combination of the HTGR with the gas turbine.

R. Cyhan: Somebody asked for the impression of the customer. We operate the Peach Bottom plant, so I would like to make a short statement and say that at Philadelphia Electric Company we believe in HTGR and are putting our hearts into making it work and produce power again very soon, and that it is making all of you proud in your faith of the HTGR concept.

GAS-COOLED FAST REACTOR DESIGN
SAFETY STUDIES AND FUEL DEVELOPMENT

(Session VII)

Chairman: C. A. Rennie,
Private Consultant

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THE GCFR DEMONSTRATION PLANT DESIGN

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ABSTRACT

This paper is primarily concerned with reporting the progress of a 300-MW(e) gas-cooled fast breeder demonstration plant design, at present under study by Gulf General Atomic. The currently proposed form of the plant is presented, together with the reasoning behind some of the more important features. The choice of operating conditions, calling for fuel and cladding temperatures now no higher than those currently proposed for LMFBR application, represents a significant departure from earlier published information on the project, and has been introduced solely in the interest of further increasing initial commonality with the LMFBR fuel program. It is noteworthy that by introduction of design refinements discussed, this change has been effected without degradation of the original performance targets. In achieving this result, the very considerable importance of proper optimization of steam and boiler conditions has been most sharply evidenced.

INTRODUCTION

This paper is primarily concerned with the continuing progress on the design of a 300-MW(e) gas-cooled fast breeder reactor (GCFR) demonstration power plant currently under study by Gulf General Atomic in association with a group of 41 U.S. utilities.

The design is based on maximum utilization of existing fuel and component technology, derived, respectively, from the Liquid Metal Fast Breeder Reactor (LMFBR) and High Temperature Gas-cooled Reactor (HTGR) development programs. In this respect, it is to be distinguished from studies of what could be done with more refractory materials, or even with the benefit only of a little further exploitation of the anticipated capabilities of the steel-clad oxide type of fuel.

In conformity with this more immediate objective, and indeed recognizing the desirability of starting with some degree of overlap in prior gas and liquid-metal technology, the present design is based on the use of thick stainless-steel-clad oxide fuel pins operated within the temperature range currently contemplated for LMFBRs.

This, in fact, represents a substantially more conservative approach than is reflected in previously published information on this reactor program. The really interesting outcome of the more recent progress reported here is that design refinement in a number of areas to be discussed has resulted in actual improvement in overall performance in the face of substantial concessions, both to secure the developmental overlap mentioned and to cope more adequately with the metal swelling problem, which at present is of so much concern to all fast-breeder designers.

In this paper, the present form of the plant and its characteristics are briefly described, but more particularly the nature and reasons for the design changes introduced subsequent to earlier published information on the subject^{1,2} are dealt with.

GENERAL PLANT DESCRIPTION

The general form of the proposed GCFR demonstration plant has not changed in essence from that already described elsewhere.^{1,2} To best appreciate the significance of those changes that have been introduced, it is desirable to start with a brief description of the present form of the plant as a whole, even though some recapitulation of earlier published information is necessarily involved.

Figures 1 and 2 show the configuration of the reactor and its associated primary circuit components, including the means for containing them, and Figs. 3 and 4 show the internals of the reactor cavity in more detail. Three main loops, each with independent boilers and circulators, are employed together with three auxiliary loops, each with its own circulator and heat-extraction system. Figures 1 and 2 illustrate the configuration of both types of loop. All boilers, or heat exchangers, and their associated

Circulators are housed in vertical wells located in the walls of a prestressed concrete reactor vessel (PCRv) surrounding the core. Gas flow is downward through the core, and it is also downward across the tube banks of the helical-coiled, once-through steam generators to accommodate the use of upflow boiling in these generators. This necessitates appropriate flow reversals in the gas path, and these are obtained by leading the core exit gas up through the otherwise vacant center region of the boiler tube bank, then down over the tubes, and up again around the boiler shells (which helps to cool the shells), and thence to top-mounted circulators discharging to the reactor top plenum.

Main circulation is by the type of integral turboblower unit developed for the HTGR, which employs a single compression stage driven by a single impulse steam turbine stage passing the entire loop steam flow. Auxiliary circulation is provided by electrically driven centrifugal circulators powered by separate and individually driven high-frequency alternators.

The reactor core (illustrated pictorially in Fig. 5), comprised of some 200 hexagonal fuel and radial blanket boxes, 10 ft in total length and each about 6-1/2 in. across flats, is supported from a top-mounted single grid plate to which each box is rigidly attached. The fuel boxes are thus clamped solely at their cold ends. They are initially spaced some 1/4 in. apart over their whole length to accommodate radiation-induced swelling and distortion. The grid plate, essentially consisting of a solid 11-ft-diam, 2-1/2-ft-thick disc with 6 in. holes to accommodate circular extensions to the fuel boxes, is itself top supported by a surrounding cylinder connected to the liner of the vessel top penetration. The entire grid plate is replaceable, if ever necessary. Tight clamping of the fuel boxes to the grid plate, as well as the means for operating variable orifices within each box, is facilitated by provision of individual small penetrations in the top access plug above each box.

Core loading (requiring depressurization) is effected by an inserted fuel machine, which lowers and traverses fuel in the vacant space below the core to a single exit port leading to a transporting cask beneath the vessel structure.

Control is by 27 rods inserted from above into fuel boxes having an appropriate central space within. Normal operation of the reactor, requiring a total reactivity swing of \$17, including a \$3 shutdown margin at all times, is provided by 21 of these rods, each of which is limited for safety reasons to 85¢ worth. The six additional rods, each of \$1.6 worth, form a backup system capable of independent shutdown.

Protection of the core liner and ducts from neutron irradiation is provided by a generous thickness of shielding. Around the core, this shielding takes the form of a removable inner layer of steel blocks, surrounded by a circular section of steel tubes containing graphite, as shown in Fig. 3. Above the grid plate, very thick shielding layers protect both the core plug above and the radial intake ducts. Cooling of the radial shielding is by a bypass from the entry helium, which is arranged to stimulate vigorous internal circulation by injector action.

A type of concrete top core access plug is used which, while not prestressed, uses the surrounding gas pressure to suppress tensile stresses under all conditions. Thrust loads are taken primarily by top compression struts, backed up by an independent "gun breech" lock acting on an appropriate vessel liner shear anchor.

Plugs of similar style are used above the boilers, in this case incorporating large central holes for circulator removal, and smaller surrounding holes to accommodate steam pipe penetrations. To avoid need for plug rotation, the breech-lock backup hold is replaced by radial bolts that are operable from the center hole after circulator removal.

Tube plugging can be done externally, the main penetration opening only being needed for complete boiler removal.

A major design criterion applied here to all penetrations is that secondary restrictors are used to limit possible depressurization rate in the event of primary barrier failure to a value that does not endanger core cooling, even in the event of a simultaneous loss of one circulator.

As shown in Fig. 6, a secondary containment is employed, but unlike the situation with LMFBRs, inerting is not required since the coolant is nonflammable. The containment is sized to reach 2 atm absolute pressure on loss of coolant, which pressure considerably aids cooling in the critical period immediately following an accidental depressurization.

The steam cycle used (Fig. 7) is noteworthy because of the use of resuperheaters following the blower turbines. This, in effect, confers most of the advantages of normal reheat, but without the need to accommodate low-density steam in the tube banks, and avoids the cost and pressure loss penalties of reheat lines leading to the main turbine. This resuperheating provides dry enough steam to avoid the necessity of providing a moisture separating turbine and gives an overall plant efficiency of nearly 38%, even though gas temperatures are limited to 542°C by decision to keep cladding hot-spot temperatures down to 700°C.

CORE THERMAL AND NUCLEAR DESIGN

Design Constraints

A number of design restraints have been adopted on the basis of previous studies and in the interest of properly representing later typical large plant requirements. The principal items are given in Table 1.

Table 1. Design Constraints for GCFR Demonstration Plant

	Present Design	Previous Design ¹
Electric power output, MW	310	330
Coolant pressure drop ratio in primary loop, $\Delta P/P$	0.048	0.048
Cladding OD/ID	1.15	1.10
Maximum temperature (mid-clad), °C	700	760
Clearance between elements, in.	0.25	0.12
Fuel smear density, % theoretical	80	88
Maximum rod rating (10% power overshoot), kW/ft	13.8	16.5

Taking these in order, plant size was, in effect, dictated by the basic fact that gas cooling is not too effective for small cores, where high neutron leakage precludes allocation of a really adequate coolant volume fraction.

The size chosen simply reflects a judgment of a minimum consistent with retention of a sufficiently representative and attractive performance, both as an economic power producer and as a test reactor. Pressure drop allocation is based on the limits of a single-stage circulator and on the loss of circulation considerations, rather than on performance optimization which indicated some benefit in harder blowing.

The coolant pressure (1250 psi) chosen is likewise not the result of "optimization" but has been set rather by what is considered a prudent increment of present HTGR requirements, within the latter's vessel-type technology.

The clad thickness chosen for the first core loadings is taken simply because most LMFBR irradiation experience lies in this area. Substantially thinner cladding is believed appropriate for the vented type of fuel rods here employed, and later loadings are confidently expected to benefit greatly from this.

Cladding temperature levels, as already mentioned, have been set to secure continuity with the LMFBR fuel program, and do not represent inherent limitations of presently available materials.

The larger interbox clearance of 1/4 in. now proposed is concerned with means for dealing with metal swelling, referred to in more detail later. An interesting point here is that this has not very significantly affected performance, thanks to the low coolant-nuclear interaction.

The reduced fuel density allowance is introduced primarily as a possible easement to control of fuel swelling at high burnup.

The linear ratings (kW/ft), being primarily dictated by fuel conductivity, are selected to be in the representative range of LMFBR oxide fuel experience.

Design Optimization

The general approach taken here may be basically characterized by employment of a radial power flattening technique which aims at reaching the same cladding hot-spot temperature in each channel, applying locally appropriate hot-spot factors, rather than aiming simply at uniform mean zone power. Thus, for instance, outer zones are operated at reduced mean power, because the flux tilt is steep there, and we desire to limit conditions on the hottest pins in the zone.

This mode of operation is of course dependent on our provision of on-load variable orificing on each fuel box and has required the development of an appropriate design computer code for its investigation.

A second characteristic of the optimization study has been the combination of this code with a boiler and steam cycle code, allowing selection of appropriate tradeoffs, particularly in choice of reactor entry temperature. An important lesson derived has indeed been that overall plant performance is very sensitive to this quantity, principally on account of the rather small "pinch point" temperature differentials between helium and steam that can easily be incurred by arbitrary choices.

Largely as a result of the foregoing considerations, we would conclude that too early "broad-brush" studies can be very misleading unless very closely watched. Both the core and the steam cycle must be studied together by one team for meaningful conclusions.

Results

The principal results are given in Table 2, together with corresponding figures relating to the earlier published design. Aside from the imposed drop in cladding temperature, the only figure that may seem to be significantly changed is the conversion ratio, which is now 1.33. The only significant reason for this low conversion ratio is the limited blanket thickness employed for the small core of the demonstration plant. Separate calculations have shown that the overall conversion ratio could be increased to at least 1.48 by lengthening the axial blanket and adding one row of

radial blanket elements. Although this thicker blanket would be too costly for the demonstration plant, it would be easily accommodated in a much larger plant.

This same scale penalty is also evident in the fuel rating, which, although improved a little, is still well below the 1 MW/kg range that our parallel studies of large cores indicate at the same temperatures.

The essential point of interest is that while the very much closer study involved in the design revision may seem to have had little effect on overall results, it indeed represents a substantial gain in the form of much easement of initial fuel developmental requirements and more tolerance for general conservatism.

This is evidenced in Tables 1 and 2 by a reduction of cladding temperature of some 60°C, accompanied by an allowance for a 50% increase in cladding thickness, a 10% reduction of needed fuel density, and provision for a large interbox gap to deal with metal swelling. That this should be possible, while actually increasing both plant efficiency and fuel rating a little, certainly represents a welcome change from the usual result of a closer inspection of initial prospects.

DETAIL FEATURES

Core Structure and Metal Swelling

The prospects of irradiation-induced metal swelling (to the tune of perhaps more than 10% by volume) is certainly of present great concern to all fast-breeder designers. There is fortunately much easement of the consequences of distortion in the case of gas-cooled breeders because they do not suffer the extreme premium on achieving compactness that is the consequence of using a coolant affecting nuclear behavior. In particular, much more widely spaced fuel rods are used, there is more liberty to space fuel boxes apart, and, also, on account of inherently lower core volumetric power density (~250 kW/liter, against typically over 500 kW/liter for LMFBRs), reactivity is much less sensitive to fuel displacement.

Fuel-box swelling and distortion, not clad swelling, is the concern. We attack the problem not by core restraint, which is capable of concealing considerable potentially dangerous strain energy storage, but by allowing growth freely to take place. The single-ended fuel support facilitates this considerably for, in association with the radial flux distribution employed, it ensures a progressive reduction in reactivity as the fuel boxes are bowed by differential growth.

Calculation shows, in fact, that with the box spacing employed, no touching of fuel boxes need be involved over the whole fuel lifetime. Only the outer ring of fuel boxes (having a high flux tilt), in fact, suffer substantial (outward) movement at the base. The consequences of this on long-term reactivity loss are provided against by provision of an appropriate clearance between the core and blanket and by adding facility to rotate fuel boxes on the occasion of each (annual) partial reload.

Calculations which relate to use of 20% cold-worked 316 steel,³ and also include the effects of thermal expansion, lead to the following results for this mode of operation:

Maximum radial displacement

At fuel element lower end	0.8 in.
At active core bottom face	0.4 in.
At core center plane	0.1 in.

Circulators

Main circulation, as already remarked, is by the type of integral series steam turboblower unit that has been successfully developed for the HTGR class of reactors.

These machines are particularly suited to fast-reactor cooling, on account of their compactness, which greatly facilitates integral containment even of very high power units. Furthermore, the fact that each machine is coupled directly to its own power source (i.e., the heat from the loop which it services) is an important factor in ensuring continuous circulation availability. This end is further facilitated by the closely adjacent location of the circulator turbines to their source of steam

(the individual boiler that they are cooling), which greatly reduces the system area needing class I protection for the guarantee of this steam supply.

Location of the series steam turbine in the high-pressure steam circuit, as opposed to its earlier application in the reheat lines of the HTGR-type reactors, introduces interesting differences, not only in the appropriate machine design approach but also in the mode of plant control called for. In the former area, the importance of protecting the thrust bearings from the high loads that could come from postulated sudden losses of steam pressure is clearly much reinforced by the high pressures used. This has led, in fact, to a design in which the circulator and turbine discs are respectively self-balanced by their own working fluids, avoiding dependence of net disc thrust on differences between helium and steam pressure absolute levels. This has been achieved by use of a pure impulse steam turbine, having large disc holes to ensure this condition, together with the application of blower discharge pressure to an appropriate area of the upstream face of the circulator disc.

Control of blower speed is by an upstream steam throttle, as opposed to use of a bypass valve, which is appropriate to the HTGRs location of the turbine in the reheat line.

Controlling this upstream throttle to keep a constant blower turbine discharge pressure at all loads has the effect of producing a helium circulation at all times closely proportional to the steam demand, thus preserving approximately constant reactor temperature rise. The main turbine throttle is thereby freed to operate independently of this consideration.

Fuel-rod Venting

The use of collective rod venting to a manifold is perhaps one of the most novel features of our approach to the GCFR design. It is therefore important to discuss both the motivations involved and the design proposed for such a system. The prime objectives of rod venting are to relieve the cladding from supporting the external coolant pressure early in life and to avoid hermetic containment of high internal fission-gas pressures when the

external pressure is returned to atmospheric, as during fuel unloading and transport and in incidents involving coolant depressurization.

Another reason for avoiding a substantial pressure differential across the cladding is that it removes a major difference between the requirements of LMFBR and GCFR fuel rods, leading to close commonality between the two programs. The fuel development program is also facilitated, because meaningful fuel irradiations can be conducted in presently available sodium-cooled fast test reactors. Other benefits of fuel-rod venting are that (1) the rods do not require extension to provide internal void space; (2) thinner cladding aids both cooling and neutron absorption; and (3) a simple facility for burst-rod detection is provided. Venting does require provisions to control released fission products, but it should be remembered that a primary circuit fission-product cleanup system is required anyway to deal with possible fuel-element failure and chemical contamination.

Figure 8 shows the venting system for the specific design contemplated. The long plenum space needed at the ends of nonvented fuel rods is replaced by a 3-in. charcoal-filled extension forming the first stage of the trap system, aimed at delaying very short-lived fission products. Connections to individual rods start with internal interconnection of the rods within each fuel box, effected by passages within an upper internal rod-spacer grid. From there, the effluent passes to a fission-gas charcoal trap located in the upper (cool) region of each fuel box. The demountable connection of the clean side of each of these traps is made at the conical interface between the fuel boxes and the bottom of the grid plate, an inflow of circuit helium is added to the effluent stream (kept below the ambient pressure, which in the seal region is about that of the core outlet coolant gas). The mixed helium and fission-gas stream is then led to the top of the grid plate via vertical drillings in the latter, each of which is fed from one or more adjacent fuel boxes. Thus, activity monitoring for each element or small group of elements permits localizing of rod failure to within a few boxes. The purpose of the diluent, or scavenge, helium addition is to ensure that any "breakaway" flow from the internal traps (due to ambient pressure fluctuations) is swept out of the pipes leading to the external cleanup traps rapidly enough to avoid substantial local decay and deposit.

Figure 9 illustrates the trapping system external to the core but within the PCRV. Basically, the effluent from the in-core traps (approximately 1 liter/day of fission gas from the whole core), plus the diluent helium, passes through the water-cooled delay beds and liquid-nitrogen-cooled traps (in the manner used in HTGRs) and returns to the main circuit blower intake, where the static pressure is well below that of the seal region and can induce an adequate diluent flow under all conditions. In this way, the need for an independent loop circulator is eliminated and a proper direction of scavenge helium flow is maintained at all times.

CONCLUSION

The general conclusion of this paper is that temperatures higher than currently envisaged in sodium breeders are far from a prerequisite for success of gas cooling and that, on the contrary, even with little or no more gains in this direction, prospects are most attractive when fuel-cycle cost, capital cost, and, above all, cost of maintenance and repair are all considered.

This is not to denigrate the advantages of pursuing further temperature raising—essential to the ultimate use of the gas turbine—but merely to point out that gas cooling appears to offer a real contemporary alternative to liquid-metal technology for breeders, in addition to tantalizing future prospects.

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Table 2. Comparative Listing of Designs

	Present Design	Previous Design
Core Geometry		
Volume, liters	3184	3432
Length-to-diameter ratio	0.5	0.6
Volume fractions		
Fuel, including center hole	0.300	0.386
Cladding	0.097	0.081
Gaps and structure	0.160	0.111
Coolant	0.443	0.422
Fuel rod diameter, cm	0.723	0.787
Number of fuel rods	30,700	28,400
Number of fuel elements	87	89
Number of control elements	31	19
Number of blanket elements	93	90
Core Physics		
Fuel average enrichment, % fissile	18.5	14.5
Radial power maximum to average	1.248	1.120
Conversion ratio	1.33	1.47
Reactivity drop per reload, \$	9.00	3.40
Performance		
Reactor inlet temperature, °C	312	283
Reactor outlet temperature, °C	542	598
MWt/kg fissile	0.605	0.576
Thermal efficiency, %	37.6	37.2
Maximum rod rating, kW/ft	12.6	15.1
Power density, kW/liter	238	243
Thermal output, MW	824	916
Electric output, MW	310	341

Fig. 1. GCFR Nuclear Steam Supply System

Fig. 2. Vertical Section through PCR

Fig. 3. Vertical Section through Reactor in Cavity

Fig. 4. Horizontal Section through Reactor

Fig. 5. Perspective of GCFR Core

Fig. 6. Secondary Containment

Fig. 7. GCFR Steam Cycle

TO MONITOR STATIONS
AND HELIUM PURIFICATION
SYSTEM

Fig. 8. Vented Fuel Element

Fig. 9. Fission-product Venting and Trapping System

GAS TURBINE POWER CONVERSION SYSTEMS FOR HELIUM COOLED BREEDER REACTORS

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ER-181

ABSTRACT

From the point of view of fuel cycle cost, helium cooled fast reactors do not require high efficiencies. Thus, the attractiveness of using gas turbines, even at the moderate gas temperatures typical of this reactor type, rests on the ability to achieve a reduction of the capital investment which is sufficient to produce lower power generation cost. Two main types of system are possible: a direct helium turbine system and an indirect system using CO₂ turbines. In the present paper the two systems are described. Problems related to the turbomachinery design and to plant control are discussed.

GENERAL

When considering power generation systems for helium cooled fast breeder reactors it is important to realize that this type of reactor produces considerably more fuel than it consumes and therefore should not simply be regarded as a power plant, but also as a kind of plutonium factory. As a consequence of the value of the fissile material produced, the fuel costs are very low and only weakly affected by station efficiency. Thus minimization of power generation cost may lead to designs featuring smaller capital investment even at the expense of significantly lower efficiency.

Using metal-clad mixed PuO₂-UO₂ fuel rods with a maximum allowable surface temperature (including hot-spots) of 800°C, reactor outlet temperatures of the order of 650°C result. These temperatures are quite adequate to provide "standard" steam conditions at the boiler outlet and

thus station efficiencies in excess of 41 % if a reheat steam turbine cycle is employed. Elimination of the reheat section of the boiler reduces the station efficiency by over 3 percentage points. However, because of the feature explained above, the resulting cost savings fully justify the use of the design with the lower efficiency.

With the above reactor outlet temperatures a further important reduction of plant efficiency must be accepted if a direct cycle helium turbine is substituted for the steam turbine power generation system. The resulting efficiency depends on the degree of heat regeneration and on assumptions concerning pressure losses in the circuit, sink temperature, etc., but for a reasonable set of assumptions it is of the order of 30 %. Again, in spite of its lower efficiency, this design may prove more advantageous if it brings about a reduction of the capital investment which is sufficient to produce lower power generation costs.

A helium turbine is a simpler, and above all, a much smaller machine than a steam turbine of the same output. The estimated weight of a single-shaft, eight-exhaust-flow steam turbine considered in our design study of a 1000 MWe fast breeder plant is approximately 2500 tons. In the alternative direct cycle version the same power output is provided by three split shaft helium turbines, the total weight of which amounts to only 1/3 of the weight of the above steam turbine. Since the cost of machines of similar character is roughly proportional to their weight, one can expect much cheaper turbomachinery in the direct cycle case.

A certain, though not very important, reduction of the cost of heat exchanging equipment can also be expected in the case of the direct gas turbine cycle since the total cost of the regenerators and coolers is expected to be smaller than the cost of the steam generators and feed-water heaters. Further reduction of the capital investment results from the elimination of helium circulators and feed water pumps and possibly from the general simplification of the ducting system. However, it is important to note that the integration of the primary loop is much more difficult in the case of the direct gas turbine cycle, since enclosing the turbomachines

together with the related heat exchangers and ducting within a prestressed concrete reactor vessel involves, in comparison with the indirect cycle design, a significant increase of the vessel size. The resulting cost increase can be only partially compensated by the elimination of the turbine house. This drawback of the direct cycle is of course not present in the non-integrated design, in which the turbogroups are arranged in the containment building around the reactor vessel. However the choice of the non-integrated design depends upon the possibility of satisfying safety requirements in case of depressurization.

It is noteworthy that in a direct cycle plant the coolers normally represent an important cost item, in our designs of the order \$ 6-8 per kW. This is because safety requires that an intermediate cooling water circuit is provided in this class of plant in order to prevent the escape of potentially radioactive gas from the primary circuit to the environment through a leak in a cooler. From the capital investment point of view this means not only that additional heat exchangers must be installed but also that the size of the primary coolers must be increased since they must be designed for only a fraction of the temperature difference originally available.

An intermediate cooling circuit is, of course, not needed in the case of an indirect gas turbine cycle but, on the other hand, heat exchangers between the primary and secondary loop and gas circulators are required. However, this design has, in principle, a number of very attractive features. First, it allows employment of a more advantageous working medium, e.g. carbon dioxide, in the secondary loop while retaining helium as reactor coolant. Secondly, the integration of the primary circuit is easier. Thirdly, the number of turbines can be reduced since it is determined solely by the maximum feasible size of a unit. Since for the same reactor outlet temperature the plant efficiency is expected to be only slightly higher than in the case of the direct helium cycle, the economic comparison of the two cycles reduces to the comparison of the capital investments involved.

DIRECT HELIUM TURBINE SYSTEM

The Cycle

The schematic of a typical direct turbine cycle for a helium cooled fast reactor is shown in Fig. 1 and the temperature-entropy diagram is shown in Fig. 2.

Helium at about 80 bars and 650°C is delivered from the reactor to the high pressure turbine where it is partially expanded to provide drive power for the compressors (2-3). From here the helium flows to the low pressure turbine (3-4) which drives the electrical generator. Leaving the turbines the helium passes through the low pressure side of the regenerator (4-5) and the precooler (5-6) in which its temperature is reduced to about 25°C . Leaving the precooler the helium passes through the two compressors (6-7 and 8-9) where its pressure is raised by a factor of approximately three. Between the compressors an intercooler is installed (7-8) in which the gas gives up the compression heat to the water of an intermediate cooling circuit. Before being delivered back to the reactor the helium passes through the high pressure side of the regenerator (9-1) where it is heated by the exhaust heat of the turbine. The cycle efficiency is 30 %.

It should be noted that although only one loop is shown in the diagram the actual helium flow is subdivided into at least three parallel loops in order to provide the required redundancy for the case when the operational turbogroups are utilized for emergency cooling.

The Design of the Helium Turbogroups

Two basic arrangements of helium turbogroups are possible: the single shaft and the double shaft arrangement. For a number of reasons it is preferable to utilize a two shaft design in which the high pressure turbine, the compressors and the starting motor are mounted on one shaft and the low pressure turbine and the electrical generator on the other. This

Fig. 1. Flow diagram of direct He-turbine cycle

Fig. 2. Temperature-entropy diagram of direct He-turbine cycle

arrangement eases both the load on the starting motor during start-up and its use as an emergency drive should it be required. However, the main attraction of the double shaft design is the possibility of providing the proper speed for the compressors while maintaining the low pressure shaft at the speed required for the generator.

At the high helium pressures required in fast breeder reactors the volume flow of the gas through the turbomachinery is very low, even for large power units. As a consequence, substantially higher rotational speeds than the synchronous speed of the generator are necessary in order to reduce the number of stages and the diameter of the unit and thus to reduce the cost of the machine. The increase of the blade speed is possible since the sonic velocity in helium is so high that the peripheral speed of the blades is limited by material stresses rather than by Mach numbers. This limitation has clearly a stronger impact on the design of the high pressure turbine than on the design of the compressors since the turbine has to work at a higher temperature. However, in the plants considered here the turbine inlet temperatures are relatively low, of the order of only 650°C , so that adequate creep strengths can be provided by the use of nickel alloys.

A 335 MWe split-shaft turbogroup designed according to the above criteria is shown in Fig. 3. Both shafts are housed in one casing. There are only two compressors mounted on the high pressure shaft in order to allow a two bearing support of the shaft and thus to ease the vibration problem. On the other hand, trade-off studies have shown that the employment of three compressors does not bring any real economic advantage. The improved thermodynamic performance resulting from the use of an additional intercooler is offset by the increased complexity of the system.

The high pressure and the low pressure turbines are arranged close to each other so that the volume of gas between them is very small. This is a very important design feature, since it allows the loss of generator load incident to be controlled by means of a simple valve arrangement.

Fig. 3. 335 MWe split-shaft He-turbogroup

All turbine and compressor discs are ferritic. However, in the zone of the first turbine-stages, a small cooling helium flow is necessary in order to keep the disc rim temperature below 550°C .

The blades of the high pressure turbine are made from Nimonic 115. This material is expected also to meet the requirements with regard to corrosion, since the use of high purity helium is assumed. Should this not be the case, a more expensive molybdenum based alloy, e.g. TZM, must be used.

The blades of the low pressure turbine are made from austenitic material, e.g. G18B. The bladings of the compressors are ferritic.

Conventional, oil lubricated journal and thrust bearings are used. To prevent excessive oil contamination of the helium, labyrinth type seals with back flushing systems are provided.

The entire turbogroup is 13.4 m long and its largest diameter is 3.7 m. The mass flow rate of helium is 625 kg/sec. Other characteristics of the machine are summarized in Tab. 1.

Table 1. Principal Characteristics of a 335 MWe He-Turbogroup

	H.P. Turbine	L.P. Turbine	L.P. Compressor	H.P. Compressor
Rotational speed, rpm	5600	3000	5600	5600
Inlet temperature, °C	653	462	24	24
Outlet temperature, °C	462	356	114	124
Inlet pressure, bars	81.8	42.2	26.6	47.6
Outlet pressure, bars	42.2	27.2	48.1	90.7
Tip diameter, m	1.46*	2.03*	1.42	1.36
Number of stages	6	6	6	8

* Last stage

An alternative arrangement of the shafts is presented in Fig. 4. This design is a consequence of layout studies which have shown that a considerable reduction of the reactor building dimensions can be achieved by arranging the high pressure shafts of the turbogroups vertically while keeping the relatively short low pressure turbines and the generators in a horizontal position. These generators will most probably be located outside the main building. Because of the rather small weight of the rotors mounted on the high pressure shaft its vertical orientation should not pose important development problems. The axial load is considerably reduced by the upward direction of the resultant thrust so that a conventional Mitchell bearing located at the upper end of the machine can be used to support the shaft.

Fig. 4. 335 MWe He-turbogroup in angular shaft arrangement

The design of the individual turbines and compressors is identical with that previously described. The casing of the high pressure shaft and the casing of the low pressure shaft are bolted together by means of a rigid bend so that they, in fact, represent a single piece of machinery. An annular duct within the bend provides the connection between the turbines. In order to obtain a uniform gas flow the annulus is subdivided into a number of sections.

The turbogroup rests on a common pedestal with the generator. It is laterally constrained but can freely move in the longitudinal direction relative to the generator in order to compensate for thermal expansions. In the vertical direction the turbogroup is not required to align with any other piece of equipment and thus is not limited in its movements. It remains still to examine a possible interaction of the vibrations of the two shafts but it is believed that this will not call in question the feasibility of the above design.

Operation and Control of the Direct He-Cycle System

Start-up

For an explanation of the start-up procedure Fig. 1 is referred to again.

With the aid of a starting motor, the high pressure turbine shaft is brought up to approximately 20 % of its nominal speed. With initially cold helium, under the system equilibrium pressure, the required electrical power is about 4 MW per group. The reactor is then brought to power and the gas temperature gradually raised. With increasing inlet gas temperature to the turbine, the speed of rotation of the high pressure shaft increases. At this stage the low pressure turbine is bypassed by opening the valve TB.

At a turbine inlet temperature of about 350-400°C the high pressure shaft reaches self sustaining conditions. As the gas temperature rises further, the low pressure turbine is started-up by gradually closing the

valve TB. Once the rated speed of the low pressure shaft is reached, the generator is synchronized and connected to the grid. By closing the bypass valve further, and increasing the reactor outlet temperature to the design value, the rated power is reached.

Power Control

For part-load operation the valve CS (see Fig. 1) is appropriately opened. This causes the mass flow rate through the compressors relative to the mass flow rate through the high pressure turbine, to be increased. As a result, the speed of rotation of the high pressure shaft sinks to a new equilibrium value. This reduction in the speed of rotation improves the part-load efficiency because of the smaller mass flow in the reactor. The return to full load conditions can be quickly achieved by closing the valve CS.

Fig. 5 shows the variation of efficiency with power. For reasons given earlier the electricity generating costs of a fast breeder reactor under part-load conditions should only be weakly dependent on the variation of efficiency. Hence the shape of the above curve can be considered as being satisfactorily flat.

Fig. 5. Variation of efficiency with power in direct cycle He-turbine plant

Generator-Trip Incident

The instant the generator loses its load, the low pressure shaft experiences an acceleration. A disturbance signal from a number of independent impulse senders (e.g. loss of load relay, acceleration regulator) leads to an immediate opening of a series of turbine bypass valves TB. It is acceptable if this occurs 0.2-0.3 secs after the load is lost, a time interval, which from past experience with air turbines is known to be easily achievable. At this time, the overspeed of the low pressure shaft has not exceeded 5 %. The turbine bypass valves are to be so dimensioned that when they are fully open, the expansion ratio of the low pressure turbine is only 1.03-1.04. Because the mass of gas enclosed between the high and low pressure turbine is small, this ratio is reached so quickly that the resulting acceleration time can hardly cause an overspeed in excess of 7-8 %.

The moment the bypass valve TB opens, the high pressure turbine is subjected to practically the entire pressure difference, so that the high pressure shaft begins to accelerate also. To prevent this, the valves CS are simultaneously opened, thereby short-circuiting the compressor flow path.

INDIRECT CO₂ TURBINE SYSTEM

The Cycle

A flow diagram of an indirect carbon dioxide turbine cycle is shown in Fig. 6 and the temperature-entropy diagram is shown in Fig. 7. This arrangement has been selected from a number of possible alternatives because it seems to be the most appropriate for the present application.

Helium at 85 bars is delivered from the reactor to a number of parallel heat exchanger units in which it gives up its heat to carbon dioxide. Circulators return the helium from the heat exchangers back to the reac-

Fig. 6. Flow diagram of indirect CO₂-turbine cycle

Fig. 7. Temperature-entropy diagram of indirect CO₂-turbine cycle

tor. Leaving the heat exchangers, CO_2 at 216 bars and about 600°C passes to the high pressure turbines (2-3) in which it partially expands to provide power for driving the helium circulators. From here the CO_2 flows to the main turbine in which it expands down from 180 to approximately 22 bars (3-4). The main turbine drives the two compressors and the electrical generator. Leaving the turbine, the CO_2 passes the first (4-5) and the second (5-6) regenerators in which it gives up its heat to the high pressure CO_2 . Now the CO_2 passes the precooler (6-7) where its temperature is reduced to 40°C . After having passed the low pressure compressor (7-8), the third regenerator (8-9) and the intercooler (9-10), the CO_2 is delivered to the high pressure compressor (pump). At the pump inlet its temperature is 25°C and its pressure is slightly supercritical. After its pressure has been approximately tripled in the pump (10-11) the CO_2 flows through the high pressure side of the second and third regenerators (11-12) and then through the high pressure side of the first regenerator (12-1). Its temperature is now about 240°C and it is delivered back to the heat exchanger (1-2). The special arrangement of the regenerators, in which the high pressure sides of the second and third regenerators are in parallel is necessary due to the strong increase of specific heat of carbon dioxide with pressure in the vicinity of the saturation line. The overall plant efficiency is 33.6 %.

The Design of the CO_2 Turbogroups

The choice of one or more turbogroups for a 1000 MWe plant depends upon the cycle parameters, the most important being the condition of the gas at the inlet to the compressor. This is because for a given speed of rotation, the maximum permissible diameter of the machine is determined by the velocity of sound in CO_2 . For a given gas density, the maximum mass flow rate of gas is thus also specified.

The velocity of sound in CO_2 is low, almost four times lower than in helium. Increasing the speed of rotation of the compressor above the synchronous speed of the generator, as is foreseen with helium turbines, would in this case be pointless. Since the maximum unit power of a turbo-

group decreases approximately with the square of the compressor rotational speed an increase of the compressor speed would inevitably lead to a splitting up of the plant into several parallel turbogroups, without offering any particular advantage. Mainly for these reasons, CO₂ turbomachines of single shaft design were chosen.

In Fig. 8, a 500 MWe, 3000 rpm single shaft turbogroup is shown. The cycle pressures given in Tab. 2 are such that they require the employment of two parallel units. In order to be able to use a single 1000 MWe turbogroup, it would be necessary to have higher gas pressures, e.g. 200 bar at inlet to the main turbine and 32 bar before the compressor.

The turbogroup consists of three components, the shafts of which are coupled together: a turbine, a low pressure compressor and a high pressure compressor. Each component is located in its individual housing. The design of the machine is entirely conventional as the requirements for the shaft sealing are considerably less stringent than in the case of the direct cycle helium machine. It is of interest to note the small size of the high pressure compressor, which is in fact a pump rather than a gas compressor.

Thanks to the low peripheral velocity of the rotors the blades are only moderately stressed, despite the high gas density. Therefore under the prevailing temperature conditions, an austenitic steel would completely fulfil the requirements of the turbine blading. The compressor blading and all the discs are made of ferritic material, perhaps slightly alloyed to increase corrosion resistance. Similarly as in the case of helium turbines, the disc rim temperature in the region of the first stages of the turbine is controlled by means of a small flow of cold gas.

The entire turbogroup and the generator are supported on a common steel pedestal. The turbogroup is free to expand in an axial direction, relative to the stator of the generator. The overall length of the turbogroup is 20.4 m and its largest diameter is 3.2 m. The mass flow rate of CO₂ is 3500 kg/sec. Other relevant data of the machine are given in Tab. 2.

Fig. 8. 500 MWe CO₂-turbogroup

Table 2. Principal Characteristics of a 500 MWe CO₂-Turbogroup

	Turbine	Compressor	Pump
Inlet temperature, °C	577	40	25
Outlet temperature, °C	343	159	40
Inlet pressure, bars	180	21	74
Outlet pressure, bars	22	76	229
Tip diameter, m	1.75*	1.21	0.60
Number of stages	6	7	4

* Last stage

Operation and Control of the Indirect CO₂-Cycle System

Start-up

The start-up of the turbogroup is achieved with the aid of an auxiliary CO₂ pump, connected in parallel with the high pressure compressor (main pump). The electrically driven supplementary pump has a volume flow rate of approximately 15 % of that of the main pump.

The start-up is carried out at a system equilibrium pressure which should be slightly above critical. Once the auxiliary pump is in operation, gas begins to circulate through the entire circuit, as the outlet pipe of the main pump is fitted with a non-return valve. Since the pressure ratio of the pump is initially very low, its volume flow is higher than nominal, about 1.0 m³/sec with a power consumption of approximately 5 MW. At this stage, the helium circulators are set in operation with the aid of auxiliary electrical motors and the reactor brought to power. As a result, the temperature of the CO₂ leaving the heat exchangers is gradually increased. Already at a temperature of 230°C the specific volume of CO₂ is about 10 times greater than at the pump inlet temperature

of 25°C. Hence, at this temperature of 230°C at the inlet to the turbine, the volume flow rate will be about 10 m³/sec. This volume flow represents approximately 30 % of the design value and is sufficient for starting the turbogroup.

From the beginning the circulator turbines gradually take over more and more of the drive to the circulator. The moment the speed of rotation of the main pump is sufficiently high to produce the same pressure ratio as the auxiliary pump, it begins to take part in increasing the CO₂ mass flow, so that from a certain point onwards, the auxiliary pump can be switched off. At this point, the self-sustaining conditions of the turbogroups are reached. As the gas temperature increases further, the turbogroup attains synchronous speed. It is then connected to the grid and brought to full power.

Summarising one can say that the large increase in CO₂ volume with temperature at a pressure which is slightly over critical allows the start-up of the system with relatively little electrical power input, as a large part of the start-up power is provided by the reactor heat.

Power Control

The power of the turbogroup is regulated by varying the CO₂ mass flow. To achieve this, the inlet temperature of the gas to the pump is raised by throttling the cooling water flow. Due to the resulting reduction in gas density, the pump outlet pressure (and consequently the pressure before the turbine) is reduced. Since the pressure before the pump has to be kept constant, a part of the CO₂ content must be removed from the circuit, as is the case with the pressure level (inventory) control method normally employed with closed cycle air turbines. The mass flow rate through the turbine gets smaller and the plant power sinks correspondingly.

It should be noted that when using this method of regulation, the operating point of the compressor will change to a higher pressure ratio. To keep the resulting efficiency variation low down to 60 % load, the de-

sign point of the compressor should be set at 80 % rather than 100 % of the nominal load.

Let us now look at the behaviour of the helium blowers under the above conditions. It is desirable to keep the gas temperatures in the reactor, at any power level, as constant as possible. This means that the CO_2 temperature before the blower turbine, also remains about the same under part-load conditions and that the ratio of mass flow rates of the two gases stays unchanged. As a result, on transferring to part-load operation, the required helium circulator pumping power becomes considerably lower than the power for the circulator turbines available at the time. In order to restore equilibrium, the circulator turbine is throttled at outlet.

With changes in the cooling water temperature, the nominal power is maintained by throttling the water flow when the water temperature sinks and by switching on the parallel connected auxiliary pump when the water temperature rises from its nominal value.

Fig. 9 shows efficiency as a function of load. Load reduction below 60 % can be reached by partially short-circuiting the compressor flow path.

Generator-Trip Incident

When the generator loses load, the resulting acceleration of the turbogroup opens valves short-circuiting the compressor and pump flow paths fully, so that the CO_2 mass flow in the circuit sinks to its idle running value at the nominal rotational speed. To avoid an over-cooling of the reactor, the blower turbine is simultaneously throttled, as in the case of part-load operation previously described.

Fig. 9. Variation of efficiency with power in indirect cycle CO_2 -turbine plant

FINAL REMARKS

A valid comparison of the two gas turbine systems discussed above and the determination of their possible economic advantages relative to the steam turbine system is certainly not a simple task. Since one is looking here for relatively small cost differences, it is important to compare optimum designs. In commercially oriented systems this means that for each variant that design must be determined which minimizes the cost of energy produced. To perform the optimizations, reliable cost information is of course required. This information, however, can be supplied by component manufacturers only on the basis of detailed designs.

Along with the optimization studies it is also necessary to compare the operational characteristics of the different systems and to determine those additional features which must be provided in order to ensure for each system the same degree of confidence with regard to plant safety. In fact, the economic impact of the operational and safety aspects may sometimes be sufficient to influence substantially the comparison.

Optimization studies of gas-cooled breeder reactors employing different power conversion systems have been performed with the help of a series of computer codes recently developed at the Swiss Federal Institute for Reactor Research. Using these codes, the technical and economic characteristics of the optimum plant can be determined, for a selected cycle, by a joint optimization of the nuclear and the power generating parts of the station.

It was the original intention of the authors to present the results of these studies at this conference. However, the results obtained so far are considered not to be sufficient to permit clear conclusions to be drawn. More investigations are still necessary and the authors prefer to reserve their judgement until these studies are completed.

DEVELOPMENT OF COATED PARTICLE
FUEL FOR A GAS BREEDER REACTOR

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ABSTRACT

The paper summarizes the work carried out in order to define a coated particle fuel for an helium-cooled fast reactor.

The first type envisaged is a modification of the coated particles developed for the HTR. Typically, the kernel is 800 microns mixed oxide, coated with PyC and SiC. On the basis of the available material characteristics, a mechanical design of such a particle has been carried out and the influence of the in-put data has been considered. This has led to a reference particle presently being developed. The paper includes the data on the fuel obtained on a laboratory scale.

As an alternative for the SiC coating, various metallic coatings have been considered : mechanical design considerations have enabled to propose a few coatings designs. The development of these coatings is in the initial stage of demonstrating the feasibility ; preliminary results are included in the paper.

Two main draw-backs have appeared from these two programs and have led to the consideration of a larger size particle (10 mm diameter) coated with a metallurgically strong coating. Various concepts are presently being compared.

INTRODUCTION

This paper outlines a preliminary study relating to a possible design for a coated particle fuel for a gas breeder reactor cooled by helium.

The following operating conditions have been adopted as guiding-line :

- Burn-up	10 % fima
- Neutron flux	$10^{23} \text{ cm}^{-2} (> 0.10 \text{ MeV})$
- Maximum gas temperature	800°C
- Power density in the fuel region	500 - 1000 MW/m ³
- Density	80 - 95 % TD
- Kernel diameter	0.8 - 10.0 mm
- Fuel	(U-Pu)O ₂ .

A preliminary scanning program is being pursued on these basic data, with no attempt to optimize.

DESCRIPTION OF THE COATED PARTICLE FOR A GBR

The fuel consists of a mixed dioxide U-Pu sphere coated by several concentric layers (Fig. 1).

The two structural layers are successively : the buffer layer and the SiC or metallic layer. As will be explained later, an intermediate layer between these two ones might be necessary and one or more protective layers can be added.

The buffer layer is presently supposed to be a low density PyC layer ; it performs two vital functions :

- it protects all the other layers against fission product recoil bombardment effects ; on that account, it should be approximately 30 microns thick for the envisaged densities ;
- it provides free volume through its internal porosity and, by irradiation induced shrinkage, permits kernel swelling and minimizes fission gas pressure.

The SiC or metallic layer assumes two functions :

- it is the pressure vessel,
- it acts as barrier for solid fission product diffusion.

MATERIALS FOR THE PRESSURE VESSEL

At the first stage, three materials were considered for a preliminary design : a vanadium alloy, chromium and silicon carbide.

Physical properties

The physical properties which are of main interest are given in Table 1.

Table 1. Physical properties of possible pressure vessel materials

Material		V	Cr	SiC	Nb
Melting point	°C	1,900	1,875	2,600	2,500
Transition temperature	°C	1,000 ^a	0-600 ^b		1,900 ^a
Melting point of the oxide	°C	660	2,400	1,750	1,500
Density	g/cm ³	6.1	7.19	3.2	8.6

^a Recrystallization

^b Ductile to brittle transition.

Chromium is undergoing the ductile to brittle transition in the operational temperature range ¹. The temperature at which the transition occurs depends mainly on the impurity content, the cooling rate, the grain size, the strain rate and the specimen preparation technique.

Vanadium, being a very oxidizable metal, has to be alloyed to elements reducing this oxidizability like aluminum, chromium or silicon. The compatibility of the alloy with the oxide fuel is fair ; with carbide fuel, a reaction zone of 70-75 microns has been detected after 1000 hrs at 800°C ².

Pyrocarbon has to be discarded, due to fast neutron dimensional changes.

The silicon carbide reacts with oxygen forming a volatile compound at low oxygen partial pressures ; vanadium and niobium respectively have the same tendency but to a decreasing extent.

Mechanical properties ^{1,3,4,5,6}

The main mechanical properties are given in Table 2, for a temperature of 800°C.

Table 2. Mechanical properties of the constitutive materials

Properties	Units	V	Cr	SiC	Buffer PyC	(U-Pu)O ₂
Young's modulus	psi	19 x 10 ⁶	36 x 10 ⁶	52.5 x 10 ⁶	2.4 x 10 ⁶	16 x 10 ⁶
Ultimate tensile strength	psi	10 x 10 ³	18 x 10 ³	46 x 10 ³	-	nd
Yield strength	psi	4.0 x 10 ³	15.5 x 10 ³	46 x 10 ³	-	nd
Thermal expansion	°C ⁻¹	9.4 x 10 ⁻⁶	5.5x10 ⁻⁶	5.35 x 10 ⁻⁶	5.4x10 ⁻⁶	10 x 10 ⁻⁶
Poisson's ratio	-	0.33	0.33	0.25-0.3	0.24	0.23
Thermal conductivity	W/m°C	38	46	36	75	3.0

The neutron flux enhances the thermal creep to an unknown extent for most materials considered. An equivalent creep rate was therefore selected and the sensitivity of the various designs to the creep coefficient was assessed. For vanadium and chromium, the following relation was utilized at 800°C :

$$\frac{\dot{\epsilon}}{\epsilon} = 1.62 \times 10^{-22} \times \sigma^{3.5}$$

and for silicon carbide (at the same temperature) :

$$\frac{\dot{\epsilon}}{\epsilon} = 4.16 \times 10^{-14} \times \sigma \times t^{-0.65}$$

where :

- σ = the stress (psi)
 t = time (second) at the given stress
 $\frac{\dot{\epsilon}}{\epsilon}$ = fractional creep rate (s^{-1}).

Nuclear properties

Figure 2 shows the evolution of the macroscopic capture cross-section Σ_c as a function of neutron energy for several elements (V, Cr, Ti, Fe, Zr, Nb). The average neutron energy is around 0.15 MeV for a spectrum representative of an helium-cooled breeder reactor.

Table 3 gives the (n, α) and (n, p) microscopic cross-sections, in millibarns, for a representative fission spectrum. From these data it appears that vanadium would give a better neutron economy and a better stability under irradiation than chromium.

Table 3. Microscopic cross-section (mb) for the
(n, α) and (n, p) reactions

Element	$\sigma(n, \alpha)$	$\sigma(n, p)$
V	0.04	1.0
Cr	0.34	1.76
Ti	2.9	2.9

The macroscopic cross-section of capture and the dimensional change of silicon carbide under irradiation are negligible.

BUFFER LAYER PROPERTIES

The buffer layer is assumed to have a density between 1.0 and 1.2 g/cm³. Table 2 gives the properties utilized for design purposes.

A densification occurs quickly under irradiation to reach a crystallographic value of 1.9 g/cm^3 after a neutron dose of 5×10^{21} . The further anisotropic dimensional changes are supposed to be counteracted by irradiative creep the layer being squeezed between the kernel and the pressure layer.

FUEL PROPERTIES

Table 2 gives the design data utilized for a $(\text{U-Pu})\text{O}_2$ fuel of 80 % TD, at a temperature of 1000°C approximately.

The volumetric fuel swelling rate is assumed to take three discrete values :

up to 2.85 % fima	$3.51 \times 10^{-2} \text{ \% / \% fima}$
2.85 to 7.65 % fima	$1.00 \times 10^{-1} \text{ \% / \% fima}$
7.65 to 12.0 % fima	$5.58 \times 10^{-1} \text{ \% / \% fima.}$

These values were interpolated from experimental swelling curves.

The fission gas and CO pressure build-up are taken into account as burn-up dependent.

COMPUTER CODE FOR MECHANICAL DESIGN

COCONUT[★] is a computer code which permits to calculate the stresses and strains in the fuel and the coating layers and has been developed at BELGONUCLEAIRE since 1967 for the HTR Fuel Program⁷.

It proceeds through successive time steps and has been written for any fuel, coating material, number of layers, temperature and irradiation evolution.

The stresses and strains in the layers are usually based on the fission gas and CO pressure, the fuel swelling and the dimensional changes induced by the thermal history and the irradiation damages. At each time step, the

[★]COating COmputation for NUclear Technology.

stresses and strains are corrected for creep effects induced by irradiation and temperature.

For high power density conditions, the stresses induced by the thermal flux through the coating are also considered, through a subroutine treatment.

PRELIMINARY DESIGN RESULTS

Temperature profile

Considering a power density of 800 MW/m^3 (fuel) with 60 v/o loading (volumetric proportion of coated particles), one obtains, for a small size (1000 microns) coated particle, a temperature difference of 100°C between the center of the fuel and the outer surface of the pressure vessel.

It appears that the high power density reached with moderate fuel temperatures is one of the most important advantages of the GBR's fuelled with coated particles and direct cooling through the bed.

Effect of the buffer thickness on the evolution of the tangential stresses

This influence has been emphasized for vanadium alloy as pressure vessel, with a kernel diameter of 800 microns, a total coating thickness of 100 microns and a coolant gas at 700 psi.

Figure 3 shows the evolution of the tangential stresses with a buffer thickness of 5 microns and a vanadium alloy thickness of 95 microns.

Up to 0.3 % fima the vessel is submitted to an external pressure, the cooling gas pressure being higher than the fission gas and the CO pressure. At 0.4 % fima, the effect of the pressure differences is alleviated as a consequence of the gap closure between the buffer and the pressure vessel. At this moment, the fuel swelling starts affecting the pressure vessel. Around 7 % fima, the evolution of the radial and tangential stresses makes the equivalent stress value pass through zero and the coating gets soon in tension. After 8 % fima, the tangential

Table 4. Coating strain at 10 % fima

Buffer thickness (micron)	Strain (%)	Diameter increase (%)
0	1.7	1.7
5	1.7	1.2
20	1.6	0

stress builds up in tension and practically levels off due to the creep phenomenon.

Figure 4 presents the same evolution but with a buffer thickness of 20 microns and a vanadium alloy thickness of 80 microns. The gap closes later (1.3 % fima). At the end of life, we have practically the same evolution as for the previous case.

For no buffer layer and 100 microns of vanadium alloy (Fig. 5), the evolution is practically the same, at the end of life, as for the first case (buffer 5 microns and 95 microns vanadium alloy).

As can be seen, the three cases give a very low tensile strength. It corresponds to less than 5 % of the rupture strength of vanadium and could not by itself be a criterion to select neither a buffer nor a vessel thickness. These results should however be interpreted with care, since swelling phenomena might occur at the high fluences and increase the stress level.

The strains at 10 % fima are given in Table 4 ; they are practically unaffected by the space partition between the buffer and vanadium layers. The diameter of the coated particles varies over the same range as the mentioned strain ; it decreases initially when the vanadium coating collapses on the densified buffer layer and increases later. Although, all the fuel beds breath to the same extent, the final expansion at the end of life depends on the buffer thickness which could be selected according to desirable bed behaviour during irradiation.

Effect of the coating material as a pressure vessel

Figure 6 gives the stress evolution in a chromium coating. The coated particle is made of a 800 microns kernel diameter, a buffer thickness of 5 microns and 95 microns of chromium. The evolution is the same as for the vanadium case with the same dimensions ; this results mainly from the adoption of a same creep law. The stresses are higher in absolute value because chromium Young's modulus is higher than vanadium Young's modulus ; the relative value is however only 2 % of the rupture strength. For what coating strain and dimensional stability of the coating is concerned the same values apply for vanadium and chromium. The only arguments for a choice between the two materials are then :

- the irradiation stability (in favour of vanadium) ;
- the compatibility with the substrate and the coolant ;
- the thermal cycling behaviour ; the chromium is favoured with a thick buffer and the vanadium with a thin or no buffer ;
- the manufacturing problems and coating qualities ;
- a better definition of the relative creep behaviour under irradiation.

Figure 7 gives the stress evolution in a silicon carbide coating. The coated particle is made of a 800 microns kernel diameter, a buffer thickness of 20 microns and 80 microns of SiC. Compared to the vanadium case, with the same dimensions, the stresses evolution is quite different. The gap closure occurs very late (8 % fima), because the SiC thermal creep is small compared to metallic thermal creep. When this gap is closed, the coating gets immediately in tension because of the thermal creep and Young's modulus characteristic of the material. The coating strain and diameter variation range are small (0.3 %). The tensile stress could be reduced by increasing the buffer thickness to an extent depending on the kernel density (Table 5) ; this would also decrease the coating strain and stabilize the diameter. The highest heavy metal loading is obtained for a density around 90 % TD. The utilization of SiC is then only

dependent on :

- the irradiation behaviour to high fluences,
- the compatibility with fuel and coolant,
- the loss of coolant safety requirements.

Table 5. Suggested buffer thicknesses with SiC coatings

Kernel density (% TD)	Relative buffer thickness (th/D)
80	0.04
90	0.05
95	0.06

FEASIBILITY TESTS

Plutonium bearing coated particle fuel has been fabricated since 1962 by the Belgian research team¹⁰. Development work on coated particle carried out in the frame of the HTR fuel program is also providing useful background.

Kernel

Three possible fabrication techniques can be selected, each one having its proper limits.

Powder routes

Agglomeration technique :

Kernels up to 8000 microns have been fabricated but a normal sphericity can be guaranteed only up to 1200 microns. Following the UO_2 route densities as low as 80 % TD can be achieved and by the U_3O_8 route, as low as 70 %.

Pressing technique :

Spheres above 2000 microns can be successfully manufactured in the density range 75-98 % TD (Fig. 10). It has to be noticed that below 80 % TD the mechanical strength will be the limiting factor.

Chemical precipitation method

By standard techniques and an oxidative calcination, densities as low as 70 % TD can be achieved with UO_2 , the particles remaining sufficiently strong ; the diameter limit is set at max. 1200 microns⁸. Undoubtedly the same results can be achieved with mixed oxides.

A Sol-Gel technique described earlier by one of the authors⁹ could also be applied even for lower densities.

Ceramic coatings

Dimensional limits

Coatings of PyC, SiC and ZrC following the classical fluidized bed technique have been successfully achieved on kernels of 800 microns and 80 % TD. The kernel diameter might be increased if the density drops below 80 % but up to now no evidence is available.

The direct deposition of other carbide coatings has not yet been investigated. Some trials to carbonize niobium deposited on PyC were not successful due to spalling of the layers. It was concluded that the carbide must be formed in situ during coating.

For high temperature coating of kernels of larger diameters, new coating equipments should be designed and developed.

Intermediate layers

For manufacturing and quality reasons, a transition layer should be incorporated between the buffer and the SiC. The following succession of layers could be envisaged (Fig. 8) :

- Porous PyC layer with a density of 1.0 g/cm^3 for fission product recoil and availability of free space, as mentioned earlier.
- A transition or scaling layer of PyC with a density increasing to 1.6 g/cm^3 or more. The total thickness of the buffer and transition layers should be 30-50 microns.
- A SiC layer with a density of 3.2 g/cm^3 and a thickness over 30 microns.
- SiC is known to be unstable under flowing gas with a low oxygen partial pressure so that a supplementary protection layer is probably necessary. This layer could be Nb, NbC or ZrC. V or W are not excluded but look less promising from the compatibility point of view. Of all materials, ZrC seems to be the most adherent and resistant to thermal shocks (thermal expansion coefficient $6.6 \times 10^{-6}/^\circ\text{C}$ versus $5.35 \times 10^{-6}/^\circ\text{C}$ for SiC).

Refractory metal coatings

The refractory metal coatings can be considered as a proper coating or as a protective layer on the ceramic pressure vessel. Metal coating can be deposited following three different techniques.

Vacuum metallization

The classical vaporization method or the plasma sputtering technique is developed by EURATOM-Ispra. Nearly no limitation exists on the kind of metal and the dimensions and weight of the particle, but the maximum industrially conceivable layer thickness is 10 microns. Tests have been performed on coated particles up to 8 microns (Fig. 9).

Electrolytic deposition

At Mol, the method has been applied successfully on fissile material for Nb, W, Mo, Fe and Ni¹⁰. The method seems to be very attractive except for the fact that the material is deposited at room temperature ; the coated particles are then under stress at operating temperature, except for vanadium. Moreover, extensive tests are necessary to prove the validity of the

technique, for what the quality of the layer is concerned (impurity, structure, etc...) ; the maximum layer thickness for quality reasons is thought to be less than 150 microns.

High temperature classical vapor deposition

The method already applied in our laboratories on Zr, Nb, W, Al_2O_3 , AlN and ZrC is applicable to most of the refractory materials. The thickness might be limited by the tendency to form a dendritic structure, but this can normally be overcome. The material is usually ductile and adherent but the same limitations on kernel size as explained previously (Cfr. page 9) are worth. Coating in a rotary furnace can however be envisaged (Fig. 10).

Preliminary irradiation test

Samples of 800 microns UO_2 kernels coated with SiC are in the BR2 reactor in a static helium atmosphere at approximately 800°C . This screening test will be stopped at a low fluence ($0.9 \times 10^{21} \text{ n/cm}^2 > 0.1 \text{ MeV}$).

CONCLUSIONS

Silicon carbide appears presently as the most favourable material for mechanically resistant layer. It imposes however the presence of a quite thick buffer (stress limitation) and a transition layer, and the utilization of small kernels (800 microns). The heavy metal loading is therefore limited to 2.4 g/cm^3 .

Metallic coatings can be designed and fabricated to reach heavy metal loadings over 3.0 g/cm^3 , since no limitations exist on kernel size. The core should however be designed to accommodate for a diameter variation of the coated particles during life up to quite large values (1.7 %).

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DETAIL OF COATING LAYERS
FOR SMALL SIZE COATED PARTICLE

FIG. 1

100 μ

FIG. 3

FIG. 4

FIG. 5

FIG. 6

SILICON CARBIDE COATING

FIG. 7

Fig. 8. SiC coatings.

Fig. 9. Nb protective layer on a small particle.

Fig. 10. Niobium coating on a 2.5 mm particle
(by decomposition of NbCl_5).

DISCUSSION

M. Dalle Donne: I do not think that the irradiation performance of coated particles in thermal reactors is completely indicative of the Ir behavior in fast reactors, due to the very big difference in fast fluence; for instance, PYC is out for fast reactors. We are also looking to metal coatings (V, Cr), but we have difficulties in making thick layers of metal (100 microns or so), 5-10 microns are no problem; bigger layers are. Could you comment on this?

H. F. Bairiot: We agree that thermal irradiations are not completely significant. They are, however, relevant as screening tests to investigate compatibility, thermal stressing, tensile behaviour, etc., in fairly representative conditions; the only real justification of those tests is budget limitation. We are also experiencing difficulties in the deposition of thick layers. Multilayer deposits are requested above 30 micron.

L. W. Graham: Since one must have low heavy metal density relative to conventional metal clad pins, what is the philosophy of using metal coatings? In your calculations a compressive force is assumed on metal coated particles from the primary circuit helium. Would not the metal be permeable to helium, so equalizing the pressure? I noticed that in the photomicrograph you have a sealing layer of relatively high density PyC on top of the buffer, presumably to prevent reaction with the kernel during deposition of the outer coating. Would not the coating be disintegrated by a fast neutron damage?

H. F. Bairiot: In a typical pin type GBR, the fuel density in the core is 2.2t/m^3 . If pressure drop considerations allowed the use of a core made of packed coated particles, the fuel density would be 2.5t/m^3 . In the range of power densities mentioned in the paper, in-and outlet coolant ducts need to be foreseen, to maintain the pressure drops in acceptable limits, so that the advantage is lost. A helium leak tightness is indeed assumed in the coating designs. The PyC transition layer is indeed unacceptable. The purpose of sealing the kernels against reaction with the SiC during deposition will be fulfilled with one of the other barriers mentioned in the paper. PyC is utilized at this modest stage of our program for it is produced routinely in our facilities.

C. B. Zitek: How are the coated particles held or contained in the reactor core?

H. F. Bairiot: The particles are impermeable annular tubes and the helium passes through the container from the outside to inside, as was described by the UKAEA in the literature.

G. Meijer: I have a principal question on the unsuitability of coated particles for gas cool fast reactors. With coated particles the heavy metal is much more diluted than in the case of pellets in a tin, i.e. normal clad fuel. The power density is very high in a fast reactor. I noticed that you take as a design basis for your particle development a core with 800 MW/m^3 . I am afraid that with this diluted coated particle type fuel, the fuel elements will burnup very rapidly with the consequence that the reactor will have to be shut down undesirably often for refueling. Can you comment on this?

H. F. Bairiot: In any reactor, the advantages of a high power density have to be balanced against the increasing penalty from down time for refuelling, as you mentioned, the higher age factors, the increasing pressure drop, etc. The power density utilized for this fuel design and development study is by no means optimized from the reactor design point of view. In fact, when the higher power density capacity is not utilized, the voidage for coolant flow can be reduced and the heavy metal density in the core increased.

C. Rennie: The main reason for looking into coated particles is the possibility of reducing the fabrication cost with that type of fuel.

LATEST CALCULATIONS FOR A GCFR WITH
A VANADIUM CLAD PIN CORE +)

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K. Wirtz +++)

2/5 4 2000

ABSTRACT

The paper gives the results of the latest nuclear and thermal calculations performed for a GCFR with vanadium alloy (V-3Ti-1Si) clad pins and oxide and carbide fuel. The helium temperature at reactor outlet is slightly above 700°C, in case of oxide fuel, which should give a reasonable plant efficiency with a direct cycle gas turbine and/or reduced capital costs. The core fissile inventory and the doubling time are 2800 and 1600 kg, and 18 and 8 years for oxide and carbide fuel respectively. The problems connected with the choice of the cladding material (creep strength, high temperature embrittlement, swelling, compatibility with fuel and coolant) are shortly discussed.

INTRODUCTION

The reasons that make gas cooling attractive for fast breeders have been discussed elsewhere.^{1,2,3,4,5} The main advantages relative to sodium-cooled breeders are the probable lower capital cost, especially with direct cycle gas turbines, the expected higher availability, easier maintenance and repair (although these two last points are difficult to quantify without a prototype) and the better breeding. Furthermore, when really high coolant temperature are available the heat sink for the gas turbines could be air or a heating system. Indeed a water reactor of 1000 Mwe has a waste of heat of about 1900 Mwth, enough to heat 400,000 homes if available at say 60°C, which is not practicle with water reactors or sodium reactors with steam turbines.

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

++) Delegated by Euratom to the Karlsruhe Fast Breeder Project.

+++) presently at the Dept. of Nucl. Engineering, Univ. of Wash., Seattle, Wash., USA

The disadvantages are the slightly higher plutonium inventory and the much higher coolant pressure with the consequent safety problems (loss of coolant accident, and inadequacy of helium natural convection to remove afterheat from the core). These safety problems can, however, be overcome by the use of a concrete pressure vessel and highly reliable and redundant coolant circulators. On the other hand, GCFR's do not suffer from safety problems of sodium-cooled fast reactors, such as big positive void coefficients and fuel-coolant reactions.

We would like here to stress again, as said before, that there are still many open questions regarding the fuel for a GCFR. The main effort should therefore be directed to fuel development and in-pile tests.

VANADIUM CLADDING

In Karlsruhe we strive as has been said, ultimately, to have helium gas outlet temperatures from the core equal or greater than 700°C , with an eye to gas turbine application or steam turbine application with rather small heat exchangers. Steels are not strong enough for such temperatures, although they could be used first at lower temperatures with a steam turbine, especially if a vented fuel concept is adopted.

A series of alloys based on vanadium, titanium, niobium, and silicon are being developed by Böhm of the Materials Laboratory of the Karlsruhe Center in collaboration with the Metallgesellschaft.^{6,7} Of these we choose the one containing 96% V, 3% Ti, 1% Si because, after 20,000 hours at 850°C , it has practically the same stress rupture strength as niobium-containing alloys and the highly neutron absorbing elements have been eliminated (10% niobium in the cladding would produce a decrease of the breeding ratio of 0.13)³

Out-of-pile creep tests performed at the Karlsruhe Materials Laboratory have shown that this alloy can stand the stresses given by the coolant gas outer pressure, the gaseous fission products inner pressure, and the pressure given by the swelling of the oxide fuel for a period of two years and temperatures up to 850°C , provided that the out-of-pile mechanical properties of this alloy are not considerably worsened by the very high fast neutron doses to which the fuel elements of a fast reactor are subjected. However, there are indications^{7,8} that, like other vanadium alloys, this one tends to maintain ductility when irradiated at high temperatures in a fast flux, or at least is much less affected than steels or nickel-basis alloys. Irradiation experiments carried out by Karlsruhe on different V-alloys indicated that no high-temperature-embrittlement occurs for fast doses ($E \geq 0.1$ Mev) up to 1.4×10^{21} and temperatures up to 750°C .⁷ These results have been confirmed by results obtained by ANL for fast doses up to 3×10^{22} and the same temperatures. Recent results at the Karlsruhe cyclotron show that a helium concentration of 1×10^{-6} atoms/metal atom, equivalent to a fast dose of about 1×10^{22} ($E \geq 0.1$ Mev), does not produce any high-temperature-

embrittlement in the V-3Ti-1Si alloy up to 900°C, although other vanadium alloys embrittle in the temperature range 850-950°C.⁸

The information on swelling of vanadium and vanadium-based alloys under a high fast flux is sparse. From the data of Wiffen and Stiegler⁹ it is possible to deduce that pure vanadium under a dose of 1.7×10^{22} (E $\geq$ 0.1 Mev) at 600°C shows a volume increase of 0.1%. Harkness¹⁰ did not find any pores in V-20 Ti after a dose of 2×10^{22} at 550°C. Both these results tend to indicate that the swelling rate is smaller than that of stainless steel.

Vanadium alloys embrittle very rapidly in the presence of oxygen and nitrogen. We have therefore looked into the compatibility problems. The main considerations are given below.

Assuming that the fuel elements remain in the reactor for two years, that helium leakage from the core is equal to 0.1% per day and that every two years the reactor is emptied of helium and filled again with commercially available helium (containing the impurities O₂ = 10 vpm, H₂O = 10 vpm, N₂ = 25 vpm), then, during their life, the pins come in contact with 1.7 reactor fillings, that is 26 vpm of O₂ and 43 vpm of N₂. Assuming that all these impurities react only with the cladding, the weight percentage of O and N impurities in the vanadium alloy at the end of the pin life will be 200 ppm, which is well below the limit where these impurities start to affect the vanadium alloy mechanical properties (3000-5000 ppm). It is clear that the amount of water coming from leakages of the heat exchangers could be considerably higher than the amount mentioned above. We recommend therefore the use of this cladding only in connection with gas turbine cycles, or with steam turbine cycles, where the steam pressure is lower than the helium pressure, for instance 130 and 120 atms respectively.

The compatibility of vanadium and vanadium based alloys containing small quantities of titanium, is good, respectively, with UO₂ and UC for temperature up to 1000°C and 900°C; it appears that the compatibility of uranium carbide with vanadium is better than with steels^{11,12}. The effect of fission products is still unclear but we hope to obtain information from our irradiations in FR2. Hofmann¹⁴ has found recently with out-of-pile compatibility experiments that the worst fission products are iodine, selenium, tellurium and cesium. The results of these tests with oxide fuel and these fission products in concentrations corresponding to burn-up of 50 to 100% are given in Table 1.

Table 1. Extent of reaction zones. UO₂ + J + Se + Te + Cs

Alloy	600°C/1000 h	800°C/1000 h
V-Zr2-Cr15	10 μ m	50 μ m
X8CrNiMoVNb1613(4988)	100 μ m	150 μ m
AISI 321 SS	150 μ m	300 μ m
Inconel 625	30 μ m	2 mm
Incoloy 800	400 μ m	2 mm

All the here simulated fission products react preferably with chromium, we think therefore that the V-3Ti-1Si alloy should be even better than the best investigated alloy (V-Zr2-Cr15).

PERFORMANCE

Table 2 shows the results of the thermal and nuclear calculations with V-3Ti-1Si clad fuel pins.

The nuclear calculations were performed with the latest cross section set of Karlsruhe, the so called MOXTOT set. Compared with the previous sets, this one is improved as follows:

- a. the latest evaluated values for $\alpha(\text{Pu})$ are incorporated (values of Gwin)
- b. the capture cross section of U^{238} have been lowered (MOXON data)
- c. the cross sections of the higher Pu isotopes have been improved.

The details of this set have been presented at the BNES conference in June 1969 in London by E. Kiefhaber et al.¹⁴

Furthermore, the Pu equilibrium composition has been modified. In the previous calculations³ this was assumed to be a mixture between plutonium coming from LWR's and GCFR's, while in the present calculation the Pu isotopic composition is the equilibrium composition from GCFR's only. This decreases the breeding ratio for oxide fuel by 0.044, while the new set of cross sections produce a decrease of 0.143. An additional 0.015 is due to the elimination of the 0.4% U^{235} in the blankets, assumed in the previous calculations. Thus the relatively low breeding ratio of 1.315 for a GCFR does not come from the V-3Ti-1Si cladding, which has as low a neutron absorption as steel, but from the use of this new set of data and from the new Pu composition.

For the oxide the helium temperature at core outlet is 720°C, while the maximum hot spot temperature at clad midwall is 850°C. For the carbide this temperature should be about the same, but it is not sure that the cladding can withstand the higher swelling pressure of the carbide and the considerable thermal stresses across the clad wall. However, if the vanadium alloy maintains its out-of-pile ductility after irradiation these stresses are relatively low. In the oxide case the pressure drop in reactor has been calculated with a constant artificial roughness on the pin walls, while in the carbide case three different roughnesses have been assumed. For continuously variable roughness, the pressure drop in the reactor would be 3.5 and 8.7 kg/cm², respectively. The roughness data are from Wilkie¹⁵ and Dalle Donne and Meerwald.¹⁶

The out-of-pile inventories and the system doubling time have been calculated with the assumptions used in the ENEA study⁴, i.e., with an out-of-pile time for reprocessing and refabrication of 0.75 years. The resulting out-of-pile inventories are very large, especially in the carbide case, and show that there is a big incentive for trying to reduce this out-of-pile time and/or increase the mean fuel discharge burnup.

Table 2. Main Parameters of 1000 Mwe GCFR

Vanadium Clad (96% V, 3% Ti, 1% Si)		
	Oxide	Carbide
Core volume (liters)	8470	4181
Fuel volume fraction	0.298	0.2845
Coolant volume fraction	0.552	0.5697
Structure mat. (16/13 SS) volume fraction	0.073	0.070
Cladding volume fraction	0.077	0.0758
Fuel pin diameter (cm)	0.74	0.835
Clad thickness (mm)	0.4	0.45
Reactor inlet coolant temperature (°C)	410	390
Reactor outlet coolant temperature (°C)	720	700
Max.nom. surface temperature (°C)	756	767
Max.fuel pin linear power (W/cm)	440	1271.7
Coolant pressure at reactor inlet (kg/cm ²)	100	120
Pressure drop in reactor (kg/cm ²)	4.8	10.2
Total thermal output (incl.heat produced in blankets) (Mwth)	2449	2778
Plant net efficiency with gas turbine cycle	40.8%	36%
Core power density (kw/liter)	267	581
Pu isotopic composition	0.8306, 0.1431, 0.0211, 0.0052	
Maximum total neutron flux (n/cm ² sec)	0.78 x 10 ¹⁶	1.32 x 10 ¹⁶
Maximum fast neutron flux (E ≥ 0.1 Mev) (n/cm ² sec)	0.46 x 10 ¹⁶	1.06 x 10 ¹⁶
Maximum fast dose (E ≥ 0.1 Mev) (n/cm ²)	1.9 x 10 ²³	2.5 x 10 ²³
Average core enrichment	13.7%	13.2%
Total fissile mass in core (kg of Pu 239 + Pu 241)	2818	1586
Average rating (Mwth/kg Pu 239 + kg Pu 241)	0.869	1.752
Mean discharge burnup (Mwd/t)	55,000	55,000
Fuel smear density (% of theoretical)	83%	80%
Internal conversion ratio = neutrons captured in core fert.mat. neutrons absorbed in core fiss.mat.	0.766	0.776
Total breeding = neutrons captured in reac.fert.mat. neutrons absorbed in reac.fiss.mat.	1.315	1.476
Breeding gain = excess Pu atoms produced total atoms fissioned	0.330	0.469
Doppler constant - T $\frac{dk}{dT}$ (T in °K)	0.731 x 10 ⁻²	0.572 x 10 ⁻²
Total reactivity in coolant (β)	0.69	0.76
Out-of-pile fissile inventory (kg) ⁺	1415	1507
System linear doubling time (years) ⁺	17.7	7.9

⁺ Calculated with: plant load factor = 0.8; out-of-pile time for reprocessing and refabrication = 0.75 years; reprocessing Pu losses = 2%.

The net plant efficiency have been calculated according to the gas turbine cycle suggested by Bammert.¹⁵ Other authors¹⁶ suggest that the optimum efficiencies would be lower, with consequent reduction in component size and capital costs.

Table 3 shows the assumptions made to obtain the efficiency indicated in Table 1. The Table shows also a solution with reduced capital costs and lower plant efficiency (33.5%). At this stage we are not able to say which is the better of these solutions, because we have no data on the capital costs.

Table 3. Gas turbine cycle data

	High capital cost solution	Low capital cost solution
Reactor inlet helium temperature ($^{\circ}\text{C}$)	410.5	343.9
Reactor outlet helium temperature ($^{\circ}\text{C}$)	720	706
Pressure drop in reactor	4.8%	4.5%
Pressure drop in rest of the circuit	5.5%	5.5%
Turbine internal efficiency	90%	91%
Turbine expansion ratio	2.585	3
Recuperator temp.difference ($^{\circ}\text{C}$)	27	45
Number of coolers and compressors	3	2
Internal efficiency of compressors (LP,MP,HP)	88%,87%,86%	88%,87%
Helium temperature at compressor inlet ($^{\circ}\text{C}$)	20	30
Plant net efficiency	40.8%	33.5%
Plant net output (Mwe)	1000	962

The helium temperature at reactor outlet in the low capital cost solution is only 706°C because a recent cladding statistical hot spot analysis based on a method developed by Amendola¹⁹ has shown that the mixed mean helium temperature at reactor outlet is 706°C , and not 720°C , mainly due to the strong power radial gradient in the outermost fuel subassemblies and in the radial blanket. The calculation takes into account of the coolant mixing as well, which is based on experiments of Baumann and Möller²⁰.

In the case of the low capital cost solution the amount of heat available for heating (water at 60°C) would be 1450 Mwth, i.e. 1245 Gcal/h.

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DISCUSSION

R. A. U. Huddle: When you did the calculations concerning the embrittlement of vanadium and its alloys by oxygen and nitrogen did you assume that the impurities reacted homogeniously with all the vanadium in the core, or did you take into consideration the temperature effect on the reactivity--particularly the effect of peak random channel temperature and hot spots.

M. Dalle Donne: We did try to take into account the temperature differences in calculating the distribution of oxygen and nitrogen in the vanadium alloy cladding. The calculation is very difficult because it depends on the dynamics of the various reactors involved, on the helium velocity, and on the distribution and diffusion of the impurities in helium. We feel, however, that the distribution of the impurities in the cladding should be fairly even, because even at the minimum clad temperature (about 450°C.) the impurities react with the cladding extremely quickly. Furthermore, the value calculated with the assumption of uniform distribution (200 PPM) is much below the dangerous level (3000-5000 ppm).

D. D. Tytgat: What is the acceptable water inleakage rate from the exchangers for the 96 percent V, 3 percent Ti, 1 percent Si alloy corrosion rate?

M. Dalle Donne: We did calculate this inleakage rate. This rate, however, should be very, very low and we think that it is very difficult and/or very expensive to make exchanges capable of guaranteeing such extremely low water inleakage rates.

P. U. Fischer: Concerning the capital cost information presented by Lsy, I would like to point out that his work is based on detailed design work and has had the benefit of a lot of industrial input including component costs.

K. Wirtz: I refer to my contribution to yesterday's panel. This paper points to the second generation of GCFBR. It shows that with metal clad pins the use of the helium turbine becomes feasible. The use of carbide fuel would remove a drawback of GCFBR; namely, the high fissile inventory. The work mainly is to demonstrate the practicality of

of advanced cladding metals and to justify their development. Presumably the immediate practical goal with respect to the GCFVR itself should be continuations of prototype development for application to the first generation reactors of the GGA Type.

L. A. Lys: Since I was referred to in the paper, I would like to comment on the difference between the cycle designs which are normally proposed in the literature and the designs proposed by the Swiss Federal Institute for Reactor Research. To explain this I would like to refer to the figure below. The figure is based on a hot spot temperature of 800°C. which is 50°C. lower than the temperature used in the paper. One can see that even with this lower temperature an efficiency of 40-41 percent can be obtained. However, compared with the optimum design at 30-31 percent efficiency an increase of power generation cost of the order of 0.35-0.50 mills per kwh results. This increase is primarily the consequence of higher capital costs related to the increased size of the heat exchanger equipment which is necessary to obtain the higher efficiency.

M. Dalle Donne: As I said the paper, we cannot say from our data which of the two gas turbine cycles is preferable, because we have no information on the capital costs. I would agree with Mr. Lys that due to the very small fuel cycle costs typical of a fast breeder, especially in the case of small plutonium inventories, the economic optimum is the direction of low capital costs. What is important, as far as we are concerned, is the fact that even with the reduced plant efficiency (33/5%) given by the gas turbine cycle proposed by Mr. Lys, we are able to get from the same core almost the same net electrical output. This is due to the fact in the lower efficiency cycle the difference between the helium temperature at core outlet and at core inlet is considerably higher, which results in a compensating effect.

GAS-COOLED FAST BREEDER REACTOR FUEL-ELEMENT DEVELOPMENT*

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ABSTRACT

A cooperative design study and irradiation test program is being carried out by ORNL and GGA to provide a metal-clad ceramic fuel pin for the GCBR. The design study indicated that if the problems associated with the high overpressure on the fuel pins from the 85-atm helium coolant can be overcome, much of the fuel and cladding technology developed on the LMFBFR program would be applicable to GCBR fuel pin design. Both sealed and manifold-vented fuel pin concepts were considered. The manifolded design has been chosen as the reference GCBR design due to its projected performance and economic potential.

The thermal flux irradiation testing of sealed fuel pins culminated with a successful test of fuel-cladding-interacting-type pins. The (U,12% Pu)O₂ fuel pins were clad with either Hastelloy X or type 316 stainless steel and operated with 700°C cladding temperatures to 60,000 Mwd/tonne burnup. No serious fuel-cladding mechanical or chemical interactions were observed in these pins. Data developed in earlier tests describing mechanical stability and fuel-cladding chemical interactions as a function of cladding temperature and coolant overpressure are also presented.

A thermal flux test of the manifolded pin design is in progress and performing satisfactorily. Fuel pins of this type are being fabricated for fast flux testing to begin in FY 1971.

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†Gulf General Atomic.

INTRODUCTION

A design study and irradiation test program is being conducted cooperatively by Gulf General Atomic (GGA) and Oak Ridge National Laboratory (ORNL) to develop fuel elements for the Gas-Cooled Fast Breeder Reactor (GCFR). The design work has been performed primarily at GGA and the irradiation testing has been implemented at ORNL. In the course of the design study, it became apparent that the only basic difference in the service condition between a gas-cooled and a liquid-metal-cooled breeder reactor is the relatively high pressure of the helium coolant in the gas-cooled system. There is some incentive in the gas-cooled concept to attain higher cladding temperatures; but they are not essential,¹ as the reference design is based on a 700°C peak cladding temperature, including all hot spot factors. This is the same as the peak cladding temperatures given for LMFBR power reactors.

FUEL PIN CONCEPTS

Several fuel pin concepts that might withstand the approximately 85-atm pressure of the helium coolant have been evaluated. The most interesting of these are listed in Table 1 along with the major limitations and advantages of each. The free-standing and the fuel-cladding-interacting (FCI) concept are both based upon a sealed fuel pin. They are very similar, with the only significant difference being that for the FCI design the cladding wall thickness is minimized to the point that slow, uniform creep collapse of the cladding can occur under the initial operating conditions. An internal pressure buildup by the release of fission gases from the fuel and minimal support from the fuel are relied upon to prevent complete collapse of the cladding. The high thermal stresses which are developed in the heavy cladding on the free-standing pin and the breeding gain penalties associated with neutron losses to the cladding provide the incentive to go from the free-standing to the FCI concept. In the FCI concept, premature creep collapse of the cladding must be prevented, and a careful design effort is required.

Table 1. Major Characteristics, Limitations, and Advantages of Gas-Cooled Fast Breeder Reactor Fuel Rod Concepts

Concept	Characteristics	Problems or Limitations	Advantages
Free-standing	Thick cladding or prepressurized Sealed can Pressure differential decreasing with burnup	Breeding penalties associated with strong thick cladding cans High thermal stresses in thick-wall cladding Prepressurization may be required	Relatively simple design
Fuel-cladding-interacting (FCI)	Long-term creep collapse of cladding Sealed can Pressure differential decreasing with burnup	Creep collapse of cladding in early life is undesirable Subject to cycling fatigue Prepressurization may be required	Economically attractive sealed pin
Manifolded-vented	Pressure equalizing Vented external to core Manifolded to common connector	Achieving tolerable leak rate at coupling Reliability of manifolding system in fuel element Potentially additional plant systems costs	LMFBR similarity Higher performance potential

The manifolded or vented-to-plenum concept reduces or eliminates the overpressure on the cladding by balancing the pressures internal and external to the fuel cladding through a manifolding system. This provides a fuel pin very similar to an LMFBR pin and permits the use of much of the fuel and cladding data developed on that program. In addition, this design approach eliminates the danger of a sudden, large release of fission gases into the primary coolant system during operation.

The economic potential for the manifolded concept includes both low fuel cycle cost and a possible net decrease in the capital cost of the GCBR plant. These lower cost estimates are based upon the potential for minimizing the cladding thickness, reducing the fuel pin and overall core length by reducing the fission gas plenum, and optimizing the operating pressures and temperatures. The cost of manifolding, fission product vent connections, and fission product monitoring stations must be added; but these are more than compensated for by the cost decreases.

Evaluation of the problems associated with these various designs and an analysis of the effort required to overcome these problems have resulted in the selection of the manifolded-vented fuel pin concept for the reference design for the GCBR. The FCI concept has been adopted as the backup design.

IRRADIATION TESTING

The in-reactor evaluation of these concepts has been carried out over the last four years in the Oak Ridge Research Reactor (ORR) pool-side facility.^{2,3} The early tests employed UO_2 fuel, whereas the last seven pins have contained $(\text{U}, 12\% \text{ Pu})\text{O}_2$. The test presently in-reactor contains the first fuel pin of the manifolded type. All of these tests are conducted in NaK-filled irradiation capsules (see Fig. 1) that provided an external pressure of 1000 psia on the test pins and thermocouple monitoring of cladding temperatures. The next tests in this program will involve seven fuel pins presently being fabricated for fast-flux testing in the EBR-II.

Capsule 03-P7.

Fig. 1. Typical GCBR ORR Irradiation Test Design.

Sealed Pin Tests

The designs employing sealed fuel pins, both free-standing and FCI types, have been evaluated in 8 capsules incorporating 16 separate fuel rods. Early results from these were reported previously.⁴ All of these tests are listed in Table 2 along with the most important fabrication and test information. Burnups have ranged from 3,400 to 60,000 Mwd/tonne. Heat ratings ranged from 7 to 22 kw/ft and cladding temperatures from 560 to 840°C.

The principal variables in these tests were the relative cladding wall strength as affected by the wall thickness, wall temperature, and cladding material, and the pressure differential across the cladding wall. This pressure differential was varied by changing the initial internal pressure (prepressurization) and the internal volume provided for the accumulation of released fission gases. The major results of interest from these tests concern the dimensional stability of the fuel pin and the fuel-cladding chemical interactions.

Dimensional Stability

The test conditions have been varied to cover the full range of cladding stability from instantaneous collapse of the cladding onto the fuel through a fully free-standing cladding. No mechanical

Table 2. Summary of Gas-Cooled Fast Breeder Reactor Irradiation Tests

Test Pin Identification ^a	Fuel					Cladding		Conditions				Significant Features or Results
	Composition	% TD ^b	Geometry	Stoichiometry (O/M)	Fuel-Cladding Gap (in.)	Composition	Thickness/OD (in.)	Time (hr)	Heat Rate (kw/ft)	Maximum Cladding Temperature (°C)	Burnup (Mwd/tonne Heavy Metal)	
GA-1	UO ₂	~ 90	P,A ^c	2.005	0.0018	Hastelloy X	0.0091/0.379	650	18/21.7	760	3,400	Specimens collapsed; negligible fuel support of cladding
GA-2	UO ₂	~ 90	P,A	2.005	0.0028	Hastelloy X	0.0092/0.378	650	18/21.7	760		
GA-4	UO ₂	~ 90	P,A	2.006	0.001/0.0025	Hastelloy X	0.014/0.342	800	18/16.2	760/673	4,500	Good condition; collapse in region of thin cladding; negligible fuel support of cladding
GA-5	UO ₂	~ 90	P,A	2.006	0.0008/0.0023	Hastelloy X	0.010-0.020/0.334-0.352	800	18/16.2	760/665		
GA-6 ^d	UO ₂	~ 95	P	2.005	0.017	Type 304 stainless steel	0.010/0.375	1,340	14.7	650	5,600	Slight creep deformation
GA-7	UO ₂	~ 90	P,A	2.003	0.0036	Hastelloy X	0.015/0.343	1,340	12.1	650	5,700	
GA-8 ^d	UO ₂	~ 95	P	2.0032	0.015	Type 304 stainless steel	0.010/0.375	2,000	13.5/15.1	700/715	8,300	Good collapse stability
GA-9 ^e	UO ₂	~ 90	P	2.0032	0.0036	Hastelloy X	0.015/0.343	2,000	11.4/12.6	650/715	8,800	
GA-10	UO ₂	~ 90	P,A	2.0039	0.0035	Hastelloy X partially surface roughened	0.015/0.343	2,200	14.0	710	11,000	Some cladding deformation under thermocouple used to monitor temperature
GA-11 ^e	UO ₂	~ 90	P,A	2.002	0.0035	Hastelloy X partially surface roughened	0.015/0.343	2,200	16.2	812	12,000	Good collapse stability
GA-12	88 UO ₂ -12 PuO ₂			1.98/1.99	0.0033	Hastelloy X partially surface roughened	0.015/0.343	1,100	15	710	4,200	Good collapse stability
GA-13	88 UO ₂ -12 PuO ₂	~ 90	P,A	1.98/1.99	0.0033	Hastelloy X partially surface roughened	0.015/0.343	1,100	18	812	4,800	Collapsed into oval shape; negligible fuel support of cladding
GA-14 ^e	88 UO ₂ -12 PuO ₂	~ 90	P,A	1.98/1.99	0.0033	Hastelloy X partially surface roughened	0.015/0.343	1,100	18	838	4,800	Cladding failed under thermocouple band; negligible fuel support of cladding. Localized collapse and failure
GA-15	UO ₂	~ 90	P,A	2.002	0.0025	Hastelloy X	0.020/0.353	~ 7,800	6.8	750	~ 16,000	Irradiation discontinued at 16,000 Mwd/tonne
GA-16	88 UO ₂ -12 PuO ₂	~ 90	P,A	1.98/1.99	0.0034	Hastelloy X	0.020/0.353	~ 7,800	8.2	750	~ 16,000	Fission-product leak detected in pin GA-16 which experienced very localized collapse and failed.
GA-17	88 UO ₂ -12 PuO ₂	~ 90	P,A	1.98/1.99	0.002	Type 316 stainless steel	0.024/0.355	~ 11,000	13.2	610 ^f	~ 47,000	Fuel pin specimens all in excellent condition; no significant deformation observed
GA-18	88 UO ₂ -12 PuO ₂	~ 90	P,A	1.98/1.99	0.003	Hastelloy X	0.015/0.343	~ 11,000	15.5	700	~ 59,000	
GA-19 ^e	88 UO ₂ -12 PuO ₂	~ 90	P,A	1.98/1.99	0.003	Hastelloy X	0.015/0.343	~ 11,000	15.5	700	~ 59,000	Fission-product emission at top of fuel pin and top of fission-product trap will be monitored; instrumented pin and trap; pin external pressure ~ 950 psi, internal ~ 1000 psi under normal operating conditions
GA-20	88 UO ₂ -12 PuO ₂	~ 90	P,A	1.98/1.99	~0.003-0.004	Type 316 stainless steel	0.024/0.355	~ 11,000	16	700	> 50,000	

^aAll fuel columns approximately 3 in. long. Gas-bonded sealed-can design, except for rod GA-20, which is manifolded with a fuel column 9.3 in. long.

^bSmear density was less than or equal to 85% of theoretical.

^cPellet, annular.

^dSpecial purpose sodium bonded design, dropped from program.

^ePrepressurized rod.

^fTemperature swing on this rod from 560 to 660°C during each reactor cycle.

ratchetting of fuel pellets with the cladding has been observed. The dimensional stability of the fuel pins varied with the cladding thickness and temperatures, as would be expected and as illustrated in Fig. 2. Here we see the final cross-sectional shape of an originally cylindrical fuel pin after irradiation under the indicated conditions. Where the initial pressure differential across the cladding was high, the cladding wall was thin, and the cladding temperature was also high, as in the first case, very severe and quite unsatisfactory collapse of the fuel pin occurred. The second cross section is from a pin that also operated with a high pressure differential and a relatively high cladding temperature, but with a thicker cladding which was more stable and collapse occurred more slowly. This particular test survived to 16,000 Mwd/tonne burnup before developing a dimple, and at that location, a crack in the cladding. When we went to lower cladding temperatures and slightly thicker cladding we found a more stable condition, and in fact, this particular pin (the third one on the figure) survived to the design goal of 60,000 Mwd/tonne burnup. Slight ovality developed, but the diameter change of less than 1% was quite acceptable. A similar pin in the same irradiation test is shown as the fourth cross section. This pin was prepressurized to lower the initial ΔP on the cladding and showed no deformation whatsoever at 60,000 Mwd/tonne.

Chemical Interactions

The chemical interactions between fuel, fission products, and cladding are generally present but not severe below 750°C, although they increase in severity at higher temperatures. In Fig. 3 we have illustrated the general trend of the reaction between fuel and cladding as a function of cladding temperature for Hastelloy X cladding on (U,Pu)O₂ fueled GCBR pins. At a cladding inside temperature of about 700°C up to 60,000 Mwd/tonne burnup there is a reaction layer about 2 to 3 mils thick; but it is a uniform reaction layer, and there is no measurable thinning of the cladding. This is considered to be a satisfactory condition. At 750°C and 16,000 Mwd/tonne burnup, subsurface voids have developed in the cladding, and these are a source of concern. At the

INITIAL ΔP , psi	800	950	950	500
CLAD T, °C	800	750	700	700
CLAD THICKNESS, mils	9	19.5	15	15
CLAD O.D., in.	0.373	0.353	0.345	0.345

Fig. 2. Hastelloy X Clad (U,Pu) O_2 GCBR Fuel Pins with Varying Degrees of Stability.

TEMP, °C	700	750	810	850
B.U. Mwd/T	60,000	16,000	5,000	5,000

Fig. 3. Chemical Interaction Between Hastelloy X Cladding and (U,Pu) O_2 in GCBR Fuel Pins.

higher temperatures the extent of the reaction increases, more voids are observed even deeper in the cladding, sometimes intergranular cracks are observed, and cladding thinning becomes measurable. This is considered an undesirable condition.

In Fig. 4 we see a comparison of the type 316 stainless steel cladding and Hastelloy X cladding compatibility with the mixed oxide fuel. This is a limited comparison at somewhat different temperatures at the fuel-cladding interface, but it represents the extent of our compatibility experience with type 316 stainless steel to date. This comparison may indicate that Hastelloy X is slightly superior to the stainless steel. On the other hand, type 316 stainless steel has shown satisfactory compatibility with the mixed oxide fuel at temperatures up to 700°C in work at General Electric Company on the LMFBR program.

R-49609

Fig. 4. Comparison of Fuel Cladding Interactions in Fuel Rods GA-17 and -18, Capsule P8.

We have examined by electron microprobe analysis the reaction layer on a Hastelloy X clad rod which operated at 700°C to 60,000 Mwd/tonne burnup. As shown in Fig. 5, we found that the distribution of iron, nickel and chromium through the reaction zone is quite similar to that observed in the surface layer on stainless steel oxidized in moist air at 800°C. The low point on each curve represents background level

Fig. 5. Strip-Chart Recordings of Microprobe X-Ray Scans Across Fuel-Cladding Interface.

(there is no plutonium in the cladding). We have not looked for fission products in the reaction zone or obtained absolute values for the concentration of the constituents. We hope that refined analyses of this type will lead to a better understanding of the reactions that are taking place and, perhaps, to means for controlling or minimizing these fuel-cladding interactions.

Summary

The sealed fuel-cladding-interacting concept appears to be quite viable at low and intermediate temperatures (below 750°C). This concept has performed successfully in the latest test in this series with the mixed oxide fuel and both Hastelloy X and type 316 stainless steel cladding. However, some of the earlier test pins (particularly one operating at 750°C and another at 835°C) failed during testing. Because of these early results and because of its higher temperature potential and similarity with the LMFBR technology, the manifolded-vented concept has been chosen as the reference for the GCBR and the fuel-cladding-interacting concept relegated to backup status.

Manifolded Pin Test

The first irradiation test of an instrumented (U,Pu)O₂ fueled manifolded fuel pin was started in the ORR in March 1970 toward a burnup goal of greater than 50,000 Mwd/tonne at a linear heat generation rate of 16 kw/ft. The cladding outer surface temperature is 700°C; the pressure external to the fuel cladding is 1000 psi, while the internal pressure is 25 psi higher. The design of the fuel pin and capsule is shown in Fig. 6. The fuel pin is a shortened version of a GCBR manifolded fuel pin with a 10-in. fuel column, UO₂ blanket pellets at the ends of the fuel column, and an internal charcoal trap. The charcoal trap would be located on the inlet coolant end of a fuel pin and is therefore maintained at 300°C to simulate the GCBR coolant inlet temperatures. The gas lines running into the top and bottom of the trap are used to collect samples of the fission gases that get out of the fuel and blanket region, into and through the charcoal trap. These also permit the maintenance of a dynamic balance between the coolant overpressure and the fuel pin internal pressure. There are thermocouples along the length of the fuel pin and in the charcoal trap region.

A preliminary analysis of the results to date from this capsule indicates very satisfactory performance of the fission product trap. Measurements under steady-state operating conditions show that the short-lived gaseous fission products decay in the trap and are not

Fig. 6. Irradiation Capsule Containing a Manifolded Fuel Pin; Capsule 04-P9.

Table 3. Initial GCBR EBR-II Irradiation Tests

Cladding Temperature (°C)	Burnup (Mwd/tonne)
700	25,000
700	50,000
700	100,000
600	50,000
650	50,000
750	50,000
800	50,000

released in significant quantity from the fuel pin. As was expected, the very long-lived gases are not significantly affected by the trap. Future measurements of trap performance will include both steady-state and pressure transient conditions.

Fast Flux Tests

We are presently constructing a set of eight GCBR fuel pins to be irradiated in EBR-II. These will simulate the manifolded design in that there will be a low pressure differential across the cladding and a large fission gas plenum and charcoal trap are included to prevent the buildup of high fuel pin internal pressures. The operating conditions for the test pins are outlined in Table 3. The pins will be operated at cladding temperatures between 600 and 800°C. The 600°C temperature in these tests will provide a common point for comparison of results with those from the LMFBR program. Three of these pins with a cladding outside diameter temperature of 700°C will permit a direct comparison with the manifolded fuel pin in our current thermal reactor test. The pins will attain cladding fast fluences up to 6×10^{22} neutrons/cm² at the maximum design burnup of 100,000 Mwd/tonne. These cladding fluences, although only one-third to one-fourth of those attained at full burnup in a GCBR, will produce significant fast neutron damage to the cladding and should answer some of the questions about the influence of this damage on the performance of GCBR fuel pins. The postirradiation analysis of charcoal fission product traps included in these pins will provide information on the ability of hot charcoal to remove condensable fission products and to delay short-lived gaseous products in the fast flux environment. Additional small, sealed containers of charcoal are included to study the effects of the environment on charcoal sorption properties.

SUMMARY

Our cooperative program with GGA has shown that a fuel-cladding interacting pin concept is very promising; however, it requires testing

in a fast-flux reactor in order to examine the effect of fast neutron damage on the cladding and how that would influence the design of a fuel pin which relies on uniform, controlled creep collapse of the cladding to maintain fuel pin integrity. The reference manifolded concept, which has greater potential, is currently under test and appears to be performing satisfactorily.

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We would like to gratefully acknowledge the numerous contributions of others, both at ORNL and GGA, which have contributed to the effort reported here. In particular, J. R. Siltanen and R. J. Campana of GGA contributed strongly to the design effort, and A. W. Longest and J. A. Conlin of ORNL provided the irradiation capsule design and operation.

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DISCUSSION

K. Wirtz: I think one has to congratulate ORNL for these results. I would like to make the following comment. The temperatures indicated in your paper would not allow a direct helium turbine cycle. For a secondary steam cycle lower temperatures would be sufficient. May I ask whether Oak Ridge has in mind to look also to advanced alloys of any type that would allow higher gas outlet temperatures.

R. B. Fitts: We are examining the maximum temperature potentials for the stainless steel cladding materials in order to allow for the recognized uncertainties in design maximum cladding temperature requirement and in order to cover the temperature region above that indicated in the present LMFBR program. We are considering the higher temperature alloys, such as the Hastelloy X and others, however, our present program does not include firm plans to evaluate these.

D. B. Trauger: We tend to be somewhat conservative at ORNL. In deference to possible hot spots in the core, it seems desirable to establish upper limits for fuel performance. This is an important reason for the tests at temperatures above 700°C.

D. D. Tytgat: In the EBR-II irradiation, what will be the ratio between the fast flux in the test reactor and the one in the 30 MWe reactor as described by Dr. Fortescue? Are ORNL and/or GGA doing some work on the coated particle fuel element design as described by Mr. Bairiot.

R. B. Fitts: As I said in the text, the fast flux will be less than that expected for the reference GCBR. This is, of course, a limitation encountered in all current fast flux tests, however, the EBR-II flux may actually provide a higher damage rate due to a different neutron energy spectrum (See Reference 3 of paper) and it will probably provide more than 1/3 of the reference fast neutron damage to the cladding. Designs other than the metal clad fuel pin are not receiving our serious consideration at ORNL at this time.

SAFETY STUDIES FOR THE GAS-COOLED FAST REACTOR

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ABSTRACT

The status of the joint East Central Nuclear Group-Gulf General Atomic program on safety aspects of the Gas-Cooled Fast Reactor (GCFR), as part of the overall GCFR Utility Program, is presented. Intrinsic and design safety features of the GCFR, studies of anticipated operational occurrences, studies of design-basis accidents, and containment studies are reviewed. The close relationship of GCFR technology to HTGR technology is discussed, and the differences between GCFR and LMFBR safety considerations are pointed out.

INTRODUCTION

For the past two years a program sponsored jointly by the East Central Nuclear Group and Gulf General Atomic has been directed toward the safety-related aspects of the Gas-Cooled Fast Reactor (GCFR) as part of the overall GCFR Utility Program, whose progress is reported in another paper¹ in this session. The safety program has four broad objectives, namely:

1. Establishing safety criteria for the design of the GCFR and its engineered safety features.
2. Developing the design bases and functional requirements for plant protective and engineered safety systems.
3. Developing analytical techniques to predict the transient response of the reactor to various fault conditions.
4. Performing safety analyses of the GCFR demonstration plant being designed under the GCFR Utility Program.

This paper presents the status of that program.

During the first year, work was accomplished in areas (1) and (3), above. In the second year, the GCFR Utility Program has focused on the design of a demonstration plant¹ and the safety program has progressively changed from preliminary studies and methods development into safety analyses of the demonstration plant design. Continued emphasis has been given to development of safety criteria and to their incorporation in the demonstration plant design.

SAFETY FEATURES OF THE GCFR

There are obvious safety advantages in the use of the inert gaseous coolant, helium. Elimination of possible change-of-phase problems and cladding-coolant reactions significantly ease the problems of engineering for overall system safety.

Important intrinsic safety features of the GCFR are the negative expansion and Doppler reactivity temperature coefficients. These override the small positive coolant temperature coefficient, resulting from decreased gas density, during all anticipated operational and accident situations. The reactor has, therefore, a net negative power coefficient.

The total reactivity worth of the pressurized helium in a GCFR is small; for the demonstration plant, it is calculated to be about \$0.38. Therefore, failure of the helium pressure containment, causing reduction in helium density throughout the core, cannot have a serious reactivity effect. Localized coolant voiding is, of course, not possible.

The reactivity effect of steam entry into the core has been investigated and found to be negative in the demonstration plant for concentrations up to 3.4 lb/ft³, corresponding to complete replacement of helium by saturated steam at the pressure vessel relief valve setting, which represents a conservative upper limit. The negative effect is due to the resonance absorption by fission products. In the initial core prior to attainment of fission-product equilibrium, the effect is also negative due to increased

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worth of the shim control rods, which are in the core during that period. This means that steam generator leakage or failures will not cause reactivity additions.

Perhaps the most important design safety feature of the GCFR is the enclosure of the entire primary coolant system in the prestressed concrete reactor vessel (PCRVR), thereby eliminating primary coolant ducts. Because of the conservative design bases, the highly redundant prestressing system, and the predictable, noncatastrophic failure modes, PCRVRs are considered by many to have a significant safety advantage over steel vessels.² As design, construction, and operational experience with PCRVRs accumulates both in this country and in Europe, wider understanding and acceptance of PCRVRs can be expected.

In this connection, the close relationship of GCFR technology to the High Temperature Gas-Cooled Reactor (HTGR) technology is worth emphasizing, especially regarding the PCRVR and the primary coolant system. The GCFR development will benefit in many areas from HTGR experience, not only with respect to design, construction, and operation but also with respect to safety and licensing experience. In HTGR experience in the U.S. to date, nonmechanistic gross failure of a PCRVR penetration closure is postulated as a design basis for engineered safety features, analogous to the non-mechanistic pipe rupture used for the same purpose in water reactors. (Gross failure of the PCRVR itself is not assumed credible.) It must be expected that a similar design-basis accident will be postulated for the GCFR, because the primary coolant system and PCRVR design are essentially similar to those in the HTGR. It should be kept in mind that gross PCRVR penetration failures are only postulated events and that inherently reliable pressure containment is provided by the PCRVR.

A design safety feature specific to the GCFR is the direct use of the steam generated to provide the helium circulation power, through series driven turbo-circulators. The coupling of the heat dump and the circulator in each loop increases the reliability of each cooling loop, and thus also the reliability of core cooling. The GCFR turbo-circulators are similar in concept and in many details to the HTGR turbo-circulators.

Another important design feature of the GCFR is the physical separation of fuel elements to accommodate fuel-element irradiation swelling and bowing due to gradients in that swelling. This increases safety by greatly reducing mechanical interference between fuel elements and by reducing the reactivity swing during an irradiation cycle.

The use of pressure-equalized fuel rods also has important effects on a number of safety areas. Most important is the elimination of fuel failure modes due to cladding collapse from high external pressure (at start of irradiation) or due to cladding deformation or rupture from internal fission gas pressure (later during irradiation).

STUDIES OF ANTICIPATED OPERATIONAL OCCURRENCES

As part of the development of overall plant safety and reliability criteria, plant fault conditions have been classified according to acceptable safety consequences and acceptable frequency-of-occurrence, also taking into account plant availability. This has resulted in identification of those operational occurrences expected moderately frequently or infrequently that will be analyzed to establish the design bases for the plant control system and the plant protective system.

Assessment of acceptable system responses to these operational transients requires an understanding of fuel-element damage thresholds. In the GCFR, fuel-cladding damage is more limiting than fuel melting in most cases. Although cladding hot-spot temperatures are limited in the GCFR demonstration plant to below 700°C (1300°F) in normal operation to assure satisfactory long-term performance, considerably higher cladding temperatures are acceptable for brief operational transients. As mentioned in the previous section, the inert gaseous coolant and the pressure-equalized fuel rod eliminates fuel failure modes due to cladding-coolant chemical reactions or to deformation from either internal or external pressure. Under accident conditions, cladding temperatures close to the melting point in the hottest fuel rods may well be acceptable.

Analyses carried out to date in this area of the safety program have included reactor plant control and stability studies. The initial studies

were directed toward establishing the kinetic response of the GCFR core. These included preliminary transient analyses for both startup and at-power conditions and a core stability analysis in which the effects of uncertainties in the reactivity coefficients were assessed and found to be minor. A nonlinear systems analysis model, GAFTRN, using digital-analog simulation techniques is presently under development. This computer code includes models for the core, blanket, and shields, including heat transfer and kinetics with reactivity feedbacks; models for the main cooling loops, including the steam generator and turbo-circulator; and models for the plant control and protective systems. The code will be expanded to treat parallel cooling loops. GAFTRN will be the principal tool used for studying anticipated operational occurrences and will be used to establish detailed functional requirements for the GCFR demonstration plant control and protection systems.

One potential operational occurrence is the failure of a steam generator tube, resulting in the ingress of steam into the primary circuit. This is not a potentially dangerous event because, as mentioned earlier, the reactivity effect will be negative. The principal rapid effect will be an increase in pressure inside the PCR. Detection and loop isolation measures are under study to avoid PCR pressure relief valve opening and to limit the amount of water leakage into the PCR in order to reduce plant loss of availability in case of steam tube leakage.

Local damage, originating in a single rod or fuel element from local coolant flow reduction, must also be considered an infrequent but possible occurrence, despite design provisions against fuel-element flow blockage. The principal concern is the limiting of propagation of local damage to adjacent elements. With no potential coolant phase-change-related phenomena in the GCFR, local damage can only be propagated by fuel melting, which would cause channel blockage and consequent reduction in cooling in neighboring rods or cause overheating in adjacent rods by contact with molten material. The time scale for propagation is determined by the time to reach melting in fuel rods with partially reduced cooling and the time for penetration of hot fuel through one fuel-element wall to an adjacent one. Studies have been made using conservative assumptions concerning heat

transfer and fuel movement and these suggest that local failure propagation in a GCFR core progresses relatively slowly and that times of tens of seconds are available for detection and reactor shutdown to limit damage to acceptable amounts. This means that signals such as main coolant loop activity may well be adequate for rapid fuel-failure detection; the suitability of such detection means is currently under active study.

STUDIES OF DESIGN BASIS ACCIDENTS

As mentioned earlier, the major design-basis accident for the GCFR has been postulated as the rapid depressurization of the primary system due to a nonmechanistic failure of a PCRV penetration primary closure with the reactor initially at full-power conditions. This accident is being used to establish the design bases for the principal engineered safety features in the plant: PCRV penetration flow restriction means, emergency core cooling systems, and reactor containment building and containment cleanup systems. The ability of these systems to cope adequately with this postulated accident will assure that ample margin exists in coping with more realistic accidents, such as slow primary system depressurization.

Following HTGR practice, flow restriction means are designed into each large penetration, structurally independent of the primary closure, to control the maximum rate of depressurization into the secondary containment. Core cooling during and after such an accident will be provided by continued operation of the main cooling loops. Main-cooling-loop reliability in this situation is increased by having the motive power for each helium circulator provided directly by steam produced from the reactor heat in its own loop. Auxiliary cooling loops, which are used for normal long-term shutdown cooling, will provide an independent, redundant backup to the main cooling loops. The redundancy and capacities of the main and the auxiliary coolant systems are sufficient to assure adequate core cooling capability for the design-basis depressurization accident, considering reasonable concurrent failures.

Inherent structural characteristics of the PCRV, together with inclusion of flow restriction means, assure that during depressurization the

normal coolant flow paths will not be interrupted and that no sizable mechanical forces will be induced on PCRV internals. Control of core temperatures during a depressurization accident requires only balancing decreasing coolant flow with decreasing reactor residual power, allowing for degraded heat transfer due to reduced coolant density. Fluid flow and heat-transfer phenomena during the accident are readily predictable and can be analyzed with little uncertainty, in contrast to situations in which coolant change of phase occurs.

To allow definitive analysis of depressurization accidents in the GCFR, a digital transient analysis code, LOCTRN, is being developed and is currently being tested. LOCTRN represents the reactor as a multivolume system with parallel flow paths depicting the primary coolant loops. The code treats depressurization into the secondary containment due to a leak from any volume, predicting the coolant discharge rate, pressures, and flows in each section of the reactor, and the transient response of the core, steam generators, and circulators at each time step. The action of auxiliary cooling loops will also be handled by the code.

To aid in the establishment of tentative specifications for emergency core cooling systems for the GCFR demonstration plant prior to completion of LOCTRN, simplified parametric studies of core temperatures during depressurization accidents have been made using digital heat-transfer codes. Partial loss of blower capacity during the accident has been considered. The effects of depressurization rate, scram delay time, and secondary containment backpressure have been investigated. Similar studies recently made for a 1000-MW(e) GCFR plant by ORNL using analog computing methods are reported in another paper at this meeting.³

Maximum cladding temperatures during a depressurization accident are plotted in Fig. 1. The conditions are: 30-sec exponential depressurization down to 2 atm pressure; scram effected 3 sec after start of depressurization; continued cooling by all main loops with circulators maintaining constant speed, constant coolant inlet temperature to the reactor.

The cladding temperature histories after scram exhibit a characteristic initial overcooling phase, during which the heat removal from the cladding

exceeds the transfer of stored and decay heat from the fuel to the cladding, followed by transition to a quasi-static equilibrium between decay heat production and coolant heat removal. During establishment of this second phase, cladding temperatures reach their highest values after several hundred seconds.

From the curves it can be seen that the transient heat removal capability of the main cooling loops is such that cladding temperature rises are acceptable. Results for partial loss of circulation capability cases also indicate acceptable cladding temperature rises, even down to a loss of two of the three main blowers. However, in such a situation, the auxiliary loops would take over core cooling well before cladding temperatures rise far above normal.

The 30-sec time constant, used in the accident shown, is the present design goal for the most rapid depressurization rate. Earlier studies indicated that this is readily attainable in 1000-MW(e) systems. In the demonstration plant, it may require smaller but still feasible flow restriction areas. Studies for a range of depressurization time constants have shown that the peak cladding temperatures, occurring after several hundred seconds when depressurization is complete, are not very sensitive to this value; e.g., the maximum cladding temperature for the case shown would be about 40°F higher for a 20-sec, instead of a 30-sec, depressurization time constant.

A more significant parameter affecting temperatures in the depressurized condition is the containment backpressure. Depressurization of the primary helium coolant into the secondary containment will result in a final overpressure in any case, the amount depending on containment volume and primary helium inventory. Since greater backpressure has a significant beneficial effect on emergency core cooling during the quasi-equilibrium phase in which peak cladding temperatures occur, there is incentive both on safety and economic grounds to keep containment volume low. Clearly, a trade-off study between emergency cooling equipment and procedures on the one hand and handling and maintenance within the containment on the other is required to establish the optimum containment size. Studies to

date indicate that at least 1 atm overpressure can be provided with an acceptable sized containment.

Historically, another class of accidents has been hypothesized for fast reactors involving core meltdown and possibly subsequent compaction into a more reactive configuration followed by explosive disassembly. This class of accidents has been extended to consider severe reactivity accidents resulting in explosive disassembly in those reactors in which credible or at least physically possible mechanisms for such reactivity insertions exist. Using gas cooling inherently eliminates the reactivity insertion mechanism due to coolant voiding. This, coupled with the provision of highly reliable emergency core cooling in the GCFR, in our opinion, allows elimination of gross core meltdown or explosive disassembly accidents as a design basis for the GCFR, as has been done for thermal reactor systems. Nevertheless, because of anticipated concern over such accidents, we have included in our program the development of capability to analyze them. Results of GCFR test cases using a modified version of the MARS code for disassembly calculations yield values similar to those for LMFBR systems. The very conservative design of PCRVs for static loading inherently provides a large dynamic energy absorption capability. Preliminary evaluations have shown that the relatively low pressures associated with core disassembly result in dynamic loading of the PCRV well within its design capability.

CONTAINMENT STUDIES

Effort is also being given to definition of design bases for containment. The depressurization accident has been used as input for determining the pressure and temperature history in the containment, using the CONTEMPT code⁴ modified for the GCFR. The energy content of the helium is low so that only small pressure overshoots above the desired final overpressure occur. To evaluate the margin for containment design pressure, the effect of concurrent steam generator rupture has been considered; a margin of a few pounds per square inch seems adequate. Containment cooling will probably not be required. These conditions give considerable flexibility in containment design for the GCFR.

Fission-product release resulting from the design-basis depressurization accident is expected to be small. Few cladding failures will occur, and the operation of the pressure-equalization system will act to limit release from failed rods.

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Fig. 1. Cladding Temperatures during Depressurization Accident in the GCFR Demonstration Plant.

DISCUSSION

M. Dalle Donne: I would like to know how you ensure the water flooding of the steam turbines which drive the gas blowers does not occur.

J. A. Larrimore: One important function of the plant control system is to ensure the proper balance between feedwater and helium flows to each steam generator during steady state and transient conditions. As mentioned in Paper 1/119, this can be performed by a control valve between the steam generator and the circulator. The control and transient studies referred to in the paper will be used to define the required control system characteristics.

W. K. Ergen: Is the gas-cooled fast reactor susceptible to compaction and reactivity increase connected with such compaction?

J. A. Larrimore: The fuel elements are separated to allow space for structural material swelling and bowing. Such a separation is acceptable only if credible core compactions would result in acceptable reactivity insertions. The most severe potential core compaction mechanism identified to date is seismic induced motion. This is being studied extensively, considering the dynamic response of soil, foundation, containment building, PCRV and support, core support, and fuel elements to seismic loadings. Current results indicate that acceptably small reactivity insertions would result even considering worst coherent fuel element movement patterns.

R. Cyhan: Have you considered a backup shutdown system for the control rods?

J. A. Larrimore: In the current design, the 27 control rods are divided into 21 shim-control rods, each having \$0.85 worth, and 6 shutdown rods, each having \$1.60 worth. The numbers of rods and worths are chosen so that the reactor can be shutdown by either set separately, allowing for an inoperative rod. An adequate shutdown capability is provided by these two sets and an additional backup shutdown system is not considered to be necessary.

SAFETY AND AFTERHEAT REMOVAL STUDIES OF THE
GAS-COOLED FAST REACTOR

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6/7/80

ABSTRACT

The temperature variations in the fuel and cladding following loss of heat removal capability, models to represent core melting and movement, and the cooling required to contain a molten core have been studied. A 1000 Mw(e) GCFR having a multicavity vessel, six main coolant loops, and two auxiliary coolant loops was taken as the plant model. The minimum mass flow to limit the maximum fuel cladding temperature to 1800°F after a depressurization to 15 psia on a 30-sec time constant was found to be almost 1% of the design value. Melting of the cladding would begin within a few seconds if the coolant were lost to 0.1% of the design value on a 1-sec time constant. Fuel melting and compaction would take several minutes, and compaction would not occur if fuel fell from the core volume. It appears feasible to contain the molten core on the vessel liner above the bottom head of the reactor vessel if the vessel liner cooling is increased to a total water flow of 25,000 gpm. The accidents examined are limiting cases having very low probability of occurrence, but should be of value in selecting the design-basis accidents.

INTRODUCTION

The general objectives of the ORNL safety studies are to determine the requirements of the protective actions and plant features necessary to insure that credible accidents will have acceptable consequences. The safety questions in the GCFR concept of interest in these studies are those concerned with the effects of a loss of coolant pressure or a loss of coolant flow. A combination of high power density and low heat capacity would be expected to produce a rapid temperature rise of the fuel following any drastic and sudden reduction in heat removal capability.

The studies to be described included an examination of the temperature variation of the fuel elements following loss-of-pressure and loss-of-flow accidents together with a determination of the

emergency cooling necessary to cope with these accidents, an examination of possible models to study core melting and movement, and an examination of the cooling required to contain a molten core.

In an effort to represent current trends in GCFR designs, the reference was taken to be a composite of several designs by Gulf General Atomic. A reasonably consistent design is needed for a feasibility study; however, many of the details are not significant in examining trends in accident behavior. The derated core design (GCFR-4D) of a 1000-Mw(e) plant described in the Alternate Coolant Task Force evaluation study, WASH-1089, was used in these studies.¹

LOSS OF COOLING ACCIDENTS AND COOLDOWN RATES

Several loss-of-coolant pressure and loss-of-flow accidents and various methods for coping with the resulting circumstances have been examined with an analog computer.² The performance of the derated core design was simulated by a representative fuel channel having the highest power density expected in normal operation with the corresponding temperature profile of this channel, as obtained from Ref. 1. The fuel channel was broken into three axial regions, or lumps, with a point model for the nuclear kinetics. The steam generator behavior was not included except that the core inlet temperature was decreased from 592 to 300°F in the first 60 sec after reactor scram to represent the probable temperature change. The mass flow was computed as a function of circulator speed, average coolant pressure, and core inlet temperature.

It was assumed that the 1000-Mw(e) GCFR would have a multicavity vessel, six main coolant loops (circulators and steam generators), and two auxiliary (or emergency) coolant loops similar to the current designs for the 330-Mw(e) demonstration plant.³

Depressurization Accidents

An exponential pressure decay to 15 psia from the original value of 1250 psia on a time constant of 30 sec was considered as

being typical of the more rapid loss-of-pressure accidents. This rate of pressure loss is representative of that of a 36,300 ft³ volume having a 90 in.² hole. A reactor scram was initiated by a flux-minus-flow signal. If all the main circulators remained in operation at full speed, the core was overcooled initially, with the cladding temperature decreasing from 1200°F to approximately 450°F and then increasing gradually to approximately 1066°F in the fuel channels having the highest expected power density.

A depressurization accident accompanied with partial loss of the circulators was examined by assuming that some of the circulators began a coastdown at the start of the depressurization and the remainder continued to run at full speed. Based on an arbitrary cladding failure criterion of limiting the peak in the cladding temperature to 1800°F, it would be necessary for the circulators that remained in operation after depressurization to provide a mass flow of almost 1% of the full pressure design value, as shown in Fig. 1. This flow capability is approximately 50% of that of all six main circulators operating at their design speed, considering the 80:1 pressure reduction and the reduced circulator inlet temperature. Such a flow capability is less than that of three main circulators, but it does exceed the capability of two main circulators or the two auxiliary circulators provided for emergency cooling if all the main circulators should fail.

Addition of secondary containment that would hold the equilibrium pressure to 30 psia would improve the mass flow capability of the circulators so that only one auxiliary circulator would be needed to cool the core, and thereby redundancy would be preserved in emergency cooling. Current GCFR design proposals include pressure-sustaining secondary containment buildings that give an equilibrium pressure of 30 psia after a depressurization accident.⁴ Examination of higher values of equilibrium pressure indicate a direct trade-off between the equilibrium pressure and the circulator capacity. It appears that an investigation of the economics of this trade-off would be worthwhile.

Limiting Loss of Flow Cases

The circulators in the current GCFR designs have very little inertia, and they can coast down in speed rapidly after a loss of motive power to produce a loss of mass flow more rapidly than a depressurization. A loss of flow was represented by an exponential decay having a time constant of 1 sec. A reactor scram was initiated by a flux-minus-circulator speed signal. With the circulators driven by steam turbines, it is difficult to envision events for a well-designed system that could lead to rapid loss of motive power to all the circulators. However, in an effort to examine limiting cases, the motive power was assumed to be instantaneously removed from different numbers of circulators, with the remaining circulators operating at full speed. This situation could also represent the coastdown of all the circulators to a minimum flow produced by auxiliary water turbines or pony motors driving the main circulators. As shown in Fig. 2, the maximum value of the cladding temperature could be held below the arbitrary failure point of 1800°F with a minimum uninterrupted flow capability of approximately 17% of that produced by all six circulators. This is less than the capacity of one of the main or auxiliary circulators. The increase in mass flow after several seconds during the 1-sec case is the result of a decrease in the temperature of the gas returning to the circulators from the steam generators.

If motive power were instantaneously removed from all the main circulators, one auxiliary circulator would have to be started within an impractical time margin of 1.5 sec after the scram to keep the arbitrary failure criteria from being exceeded. Increasing the time duration of the coastdown would have the advantage of greatly reducing the requirements placed on the emergency core cooling systems. For instance, in the case of uninterrupted minimum flow, increasing the time constant for the coastdown from 1 to 5 sec (also shown in Fig. 2) allowed the minimum flow capability to be reduced from 17 to 1% (1.52% mass flow), which could be readily achieved with auxiliary drive motors for the main circulators. Figure 2 also shows that a further lengthening of the time constant to 10 sec reduces the peak to 1342°F for 1% flow capability.

Figure 3 shows how the increase in the time constant to 5 sec would also allow the maximum time interval for initiation of startup of one of the auxiliary circulators to be as long as 30 sec, an interval that is comparable to the startup times for emergency core cooling systems in light-water-cooled power reactors. A further lengthening of the time constant for the coastdown to 10 sec would allow the time margin for starting up auxiliary circulators to be extended to 100 sec.

Cooldown Following a Reactor Scram

A normal reactor scram with the coolant mass flow held constant produced cooldown rates that might be rapid enough to cause concern for thermal cycling of the core and steam generator. The coolant temperature at the core outlet dropped from 1082 to 650°F in 17 sec, as shown in Fig. 4. A ramp reduction in circulator speed, as shown in Fig. 4, or purposeful coastdowns of all the circulators after a scram might be used to limit the cooldown rate to a reasonable value. Acceptable rates of temperature change for items such as the fuel and cladding, primary loop ducting, and steam generator components need to be established. A coolant flow reduction on a time constant somewhere between 5 and 10 sec would probably give a system cooldown rate of 1 to 3°F/sec, which approaches the limit for one type of steam generator.⁵

MODELS FOR CORE MELTING AND MOVEMENT

The temperature variation as a function of time throughout the core following a loss of cooling ability has been examined by means of the CHEMLOC program.⁶ This program from the Code Center at the Argonne National Laboratory was adapted to the IBM 360/75 computer for the necessary calculations. The program had been originally prepared for studying the melting of water-cooled cores after a loss-of-coolant accident, and modifications were made to account for helium rather than water (or steam) as a coolant, as well as to permit more realistic temperature profiles.

In these calculations, the coolant mass flow was attenuated exponentially on a 1-sec time constant down to an asymptotic value of 0.1% of normal. (This lower limit was chosen to prevent instabilities that appeared in the CHEMLOC program at zero flow.) At the instant attenuation began, all fission power production was assumed to be terminated, and the only source of power was taken to be that from the decay of fission products from an end-of-life core. The core was divided into eight radial zones, numbered 1 through 8 from inner to outer, each containing an equal volume. Each radial zone was further subdivided into 13 axial zones. Figure 5 contains an indication of the time required after the initiation of the loss of mass flow for the hottest fuel cladding to reach various temperatures in each radial zone. After a zone achieved a temperature of 5000°F, the fraction of fuel that melted was taken to be proportional to the amount of heat further deposited in that zone. Under the assumption that the fuel did not move, the time intervals after the beginning of the loss of flow for half the fuel in each zone to melt are shown in Fig. 6.

Melting was found to begin near the center of the axial length of the fuel rods. In one model of the analysis, it was assumed that the molten material fell to the bottom of the core and collected on the boundary of the core and lower blanket without leakage. The control rods remained inserted without melting. The molten material was added to the solid material already there in order to obtain the amount of the core that had reached full compaction. The unmelted fuel pellets above the melted zone were assumed to fall to the lower interface of the melted zone, where they would be supported by the jammed solids below. A radial zone would then consist of a compacted region at the bottom, a region of unmelted jumbled fuel above it, and then a void extending to the top. No attempt has been made to relate this sequence of events to physical reality. Reactivity calculations were performed by means of the DOT computer code.⁷ This model was found to become critical when approximately 77% of the fuel was in the compacted region of melted and unmelted material, and 23% was in the jumbled region

above the compacted region. The approach to this condition would require several minutes.

Another model being examined is based on the assumption that melting of the fuel cladding allows the unmelted fuel pellets to fall to the boundary of the core and lower blanket. Preliminary calculations indicate that this collection of fuel from a jumbled core could go critical, resulting in rapid melting of this collection. Further compaction of the melted fuel would raise the reactivity at a higher rate. This model appears to give the upper boundary to the energy release from criticality caused by core compaction.

Previous models for calculating the reactivity excursion from the melting and compaction in small fast reactors have assumed that large portions of the core compacted in a gravity fall.^{8,9} The models described above represent an attempt to establish more realistic descriptions of core compaction to determine the maximum hypothetical nuclear excursion. These models were developed to provide the basis for estimating the limiting reactivity rates from compaction of the core in order to establish an upper limit on the energy release. In an actual meltdown, some of the molten fuel, as well as some of the unmelted pellets, would probably fall from the core region and criticality might never be reached.

CONTAINING AND COOLING THE MOLTEN CORE

The fuel, cladding, blankets, and core internal structural members were assumed to fall onto the bottom thermal shield 10 sec after reactor shutdown at the start of the accident. The heat-generation rate in the fuel and blanket at any time was assumed to be that from decay heat only. In this situation, the bottom thermal shield would melt in about 30 min. If the heavy oxide and fission products from the core and axial blankets were to settle to the bottom of the molten material in a uniform layer without vaporization, a slab about 5 in. thick with an initial internal heat-generation rate of 49 Mw(t) or approximately 10^6 Btu/hr · ft³ would form. The vessel liner and

subsequently the concrete would be subjected to molten UO_2 at 5000°F . Since the normal vessel liner cooling water flow rate is not adequate to remove the heat, the molten fuel would penetrate the bottom head of the reactor vessel. If the concrete were to spall off and float in the molten UO_2 , the 12-ft bottom head could be penetrated in about 37 min. On the other hand, if the fuel were supported until the concrete melted, it would require about 5 hr for the fuel to penetrate the head.

It appears feasible to contain the molten fuel inside the vessel liner if the vessel liner cooling is increased. The fuel would freeze on the cold surface and provide an insulating layer that would limit the heat flux into the vessel liner. It would be necessary to provide adequate cooling on the floor and lower sidewall of the vessel liner to remove heat from the frozen UO_2 layer and maintain the integrity of the vessel liner. The heat flux is approximately $750,000 \text{ Btu/hr} \cdot \text{ft}^2$. The heat radiated from the top of the molten pool would melt all the thermal shields. The cooling water flow would also have to be increased on the vessel liner above the molten pool to remove the heat deposited by radiation and convection. A total water flow of approximately 25,000 gpm under the vessel floor and up the sidewalls for 15 ft would remove the heat being generated with a 50°F rise in the water temperature. The required flow would be equivalent to a sheet of water 2 in. thick flowing at a velocity of 10 ft/sec with a path length of approximately 25 ft.

A method for increasing the surface area for cooling the molten fuel would be to subdivide the vessel floor into deep, narrow passages with cooling water in tubes inside the partition walls to freeze the fuel as the molten material fell into the vessel cavity. This approach would minimize the meltdown of the thermal shields and reduce the heat input to the upper part of the vessel. However, this arrangement would cause structural complications and probably be more costly than increasing the cooling water flow on the floor and lower sidewalls of the vessel liner.

CONCLUSIONS

The accidents examined are highly unlikely, but were thought to be appropriately severe. They may or may not be representative of those that may be eventually considered as design-basis accidents. Obviously, the design-basis accidents must be selected before the actual functional requirements can be established for the emergency cooling systems. Failure criteria for the fuel and cladding need to be established.

The studies have been instructive, and even these preliminary results should be helpful in evaluating the concept of the GCFR. Systems to cope with the limiting cases of the accidents examined are within the realm of engineering design. Systems to cope with more realistic models of each accident examined should place even less demands on the design.

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Fig. 1. Depressurization with Minimum Coolant Flow to Prevent Cladding Failure.

Fig. 2. Examples of Loss of Coolant Flow at Full Coolant Pressure.

ORNL-DWG 69-12460

Fig. 3. Loss of Flow at Full Pressure on a 5-sec Time-Constant with Startup of One Auxiliary Blower.

ORNL-DWG 69-12461

Fig. 4. Cooldown Following a Reactor Scram.

Fig. 5. Time for Hottest Cladding to Reach Various Temperatures Following Loss of Cooling.

Fig. 6. Time (sec) for Fuel in Core to Reach a Half-Melted Condition.

DISCUSSION

D. B. Trauger: As the papers and the discussion of this session indicate, the important problems for the GCFBR are subject to rather straight forward engineering analysis and testing. It should be possible through a modest effort to understand these problems. With such results at hand and the reassessment of the indicated economic advantages, it then would be possible to effectively evaluate the concept. At that time a decision should be made that the GCFBR is either impractical and should be dropped or is attractive and should be pursued. The important point is that the concept can be evaluated by rather straight forward methods and a modest effort. Since this is possible without much effort it should be done.

DISCUSSION

W. F. Häussermann

ASPECTS OF GAS-COOLED FAST REACTOR DEVELOPMENT AT ENEA

In January 1968 the ENEA committee of Experts on Co-Operation in the Reactor Field, known as the Top Level Group met to examine what scope there was for co-operation between ENEA Member Countries to develop and exploit advanced power reactors. The Top Level Group concluded that any joint effort would best be directed towards promoting the construction of heavy water power reactors and extending the scope of the development of fast reactors.

Up to that time, an enormous amount of FR work in Europe had concentrated on sodium cooling, whereas the potential of other coolants had been treated only briefly. It was decided that, because a large amount of proving remained to be done before sodium could be confirmed as a good solution to the problem of cooling a fast reactor, it was desirable at least to assess the possibilities of using steam and gas as alternative coolants.

The cost of such assessments would prevent Western European countries from embarking on separate national programmes, especially since it would create competition for money already being spent on sodium cooling. Thus the investigation of alternative coolants for fast reactors appeared very suitable for international co-operation where governmental as well as industrial interest would be involved.

As a first step, two specialist teams were set up by ENEA to evaluate the merits of steam and gas cooling relative to sodium. The technical basis for the studies was contributed from national studies done in Belgium, Germany, Sweden, Switzerland and the United Kingdom. In addition, the preliminary conclusions of a similar exercise done by the USAEC in collaboration with American industry were made available. Industrial firms were encouraged to participate in the work of the two specialist teams and industry's formal association was assured through Foratom.

It soon became clear that fuel should be given priority for development for both gas and steam cooled fast reactors. The members of these expert teams also agreed that there was a need for a large fuel test facility which might also be conceived as a prototype power station. A number of fuel-cladding options were studied, including various nickel-based alloys for the SCR prepressurized to withstand the force of the coolant pressure, and also "strong clad" and "weak clad" (vented) fuels for the GCR although for this reactor particular interest is being shown in fuel designs using coated particles with a silicone carbide outer layer, as developed by the Dragon Project for the HTR.

To streamline a co-operative development programme, some reduction was obviously necessary in the options available. One step in this direction was the stopping of work on steam cooling for fast reactors, first in the U.S.A. and more recently in Germany, and development of this variant is unlikely to be taken any further within the framework of ENEA. Still, to establish a sound basis for future decisions on gas-cooled fast reactors, the experts estimated that a considerable body of experimental results was needed in support of three fuel concepts; strong clad, vented, and coated particle and on two types of coolant; CO_2 and helium.

The actual development of gas-cooled fast reactor fuels would certainly be done in national laboratories under government sponsorship where the necessary expensive facilities already exist. We feel that if a closed intergovernmental group of fuel testing were formed, under the auspices of ENEA, it could not only co-ordinate these national programmes, but also reinforce the private industrial efforts now drawing up the technical justification for a full-scale gas-cooled fast reactor installation.

An attempt was made by ENEA in Spring 1969 to devise a voluntary distribution of the essential development work amongst the industrial and governmental establishments of interested countries. Unfortunately, it was not possible to obtain agreement upon an immediate joint programme, because there were large differences of opinion as to the justifications for following up the potential of coolants alternative to sodium.

For example, one body of opinion held that there was not a strong enough reason for diverting some of the effort which is at present deployed on sodium reactor development; and that work on an alternative coolant was admissible only as a supporting measure to insure against unforeseen difficulties in the application of sodium technology on a large scale. Thus this group believed that it was advisable to concentrate for years yet on sodium cooling before devoting much financial and scientific effort to developing alternative coolants.

On the other hand, an equally strong body of opinion held that the ENEA evaluation had revealed sufficient potential advantages to justify the development of an alternative coolant in its own right, irrespective of the success or problems of sodium. This school of thought believed that the gas cooling concept had ample scope for sharing in the future fast reactor market with sodium and that there was merit in maintaining the principle of choice which has evolved in the thermal reactor market.

Successful commercial development of the thermal HTGCR and coated particle fuels is clearly important for the future of gas cooling for fast reactors, and hence the next two years are likely to be crucial in this respect. We hope that within this time scale, it will be possible to organize a viable international project for the development of gas cooling for fast reactors.

As an initial step, a Working Group on Gas-Cooled Fast Reactors has been set up by ENEA to exchange information and to formulate more precise plans for future work. Eleven European countries, the U.S.A., and Japan, as well as Euratom and Foratom participate in this Working Group which met in May and in December 1969 and published a Status Report on Gas-Cooled Fast Reactor Development in November 1969. This Group will make recommendations at its next meeting in Autumn 1970 as to the co-ordination of future work in Europe on the basis of developments in Member countries during the first half of 1970.

FAST BREEDER REACTORS
NOT LMFBR OR GCFR — BUT FBR

Compton A. Rennie*

Systems analysis studies carried out in various countries all show that there is a cost incentive to introduce fast reactors into the nuclear and fuel cycle costs and the predicted performance. The actual cost savings and the time at which these fast reactors are introduced will of course depend on the costs and performance which can actually be achieved.

It must also be remembered that when these fast reactors are ready to be introduced they will have to compete with the advanced converter reactors available at that time and unless they have sufficiently good costs and performance their introduction will be delayed. Eventually fast reactors must be introduced because of limitations in uranium supplies but in order to obtain the maximum cost benefit, it is necessary to have a good fast reactor available as early as possible.

These same systems analysis studies show that with the estimates now available for capital and fuel cycle costs of reactors, which will not be built in quantity until the 1980's at the earliest, it is not possible to make a clear-cut choice between the sodium cooled fast reactor (LMFBR) and the helium cooled fast reactor (GCFR) as the limits of uncertainty would alter the preference from one to the other.

It is important to have a good fast reactor available at that time as the cost benefit to the nuclear power industry could be several billion dollars at present day prices. Largely for historical reasons the current emphasis is on the LMFBR type and there is a real danger that not enough effort will be put into the GCFR type to enable a proper comparison to be made so that the customer can be in a position to choose the better type for his own use.

* COMPTON A. RENNIE was Chief Executive of the OECD High Temperature Reactor Project, the DRAGON Project, from 1959 to 1968, and is now a private consultant. He has recently been associated with the gas cooled fast reactor work being carried out by Gulf General Atomic in San Diego and by the Gas-cooled Breeder Reactor Association in Brussels, and worked on sodium cooled fast reactors in the UKAEA during the period 1950 to 1957.

HISTORICAL BACKGROUND

Over ten years ago when fast reactor programmes were adopted in France, Germany, the UK, the US and the USSR, there was no technically acceptable alternative to the sodium cooled fast reactor. Each of these countries has an LMFBR programme and because of the desire to introduce fast reactors in the 1980's there is a large programme in each country, albeit with considerable duplication of effort. The decision to embark on a new coolant technology, sodium cooling, was taken more than ten years ago not because it looked an easy technical problem but because it seemed to be the only way to introduce fast reactors. In fact some of the earlier practical experiences were discouraging but the work was pushed ahead because of the incentive, and the money and effort allocated to it have been steadily increased as the technical problems have proved difficult to solve.

About ten years ago two significant developments took place. One was the extension of work on a new reactor coolant technology, helium cooling, which is now being applied to the High Temperature Gas-cooled Reactor (HTGR), and the other was the acceptance of a new pressure vessel technology, the prestressed concrete reactor vessel, which permits higher operating pressures and limits depressurization rates of the primary gas circuit as compared to large steel pressure vessels. These two developments have now been brought together to produce the HTGR programmes now under way in Europe and the USA. The first large commercial HTGR power stations are likely to be ordered within a year or so and will provide a practical confirmation of helium reactor technology. There are already carbon dioxide cooled power reactors with prestressed concrete pressure vessels operating in Europe and there will soon be a helium cooled power demonstration HTGR reactor operating in the US.

Another change which has taken place during the last ten years is a gradual increase in the size of power plants, now about 1000 MWe and likely to go to 2000 MWe units, and these large unit sizes favour the nuclear performance characteristics of GCFRs. The current GCFR programmes are extensions of the HTGR reactor technology with a different fuel concept. In fact the current fuel concept for the GCFR is based on the LMFBR pin type fuel

and the current designs are based on making the fuel operating conditions as similar as possible, using an indirect steam cycle power plant. Another fuel concept based on existing HTGR fuel technology is also being worked on but this coated particle fuel concept is currently seen as a longer term and possibly cheaper alternative to the pin type fuel, with the potential of being able to be used with direct cycle helium gas turbine power plants.

The use of helium cooling in a prestressed concrete reactor vessel enables a technical performance to be achieved which is comparable to or even better than that for sodium reactors with similar fuel, bearing in mind that both studies are still paper studies. This equivalence was not known ten years or more ago and it is an interesting speculation as to whether there would have been so many LMFBR programmes in existence today if it had been known, or whether there might now be some LMFBR and some GCFR programmes, or even all GCFR programmes. The middle alternative would anyway be the most satisfactory as it is very desirable to have competition between fast reactor types as well as competition between different manufacturers of the same type.

PRESENT STATUS

There are currently major research and development programmes on sodium cooled fast reactors in France, Germany, Italy, Japan, the UK, the US and the USSR and the total annual expenditure is probably over 200 million dollars. This expenditure has been going on at a smaller level for several years and is likely to continue at the same general level for the next ten years. Probably about 1000 million dollars has been spent on sodium cooled reactor technology and fuel development up to now and it is likely that some 2000 million dollars will have been spent over the next ten years.

Several demonstration LMFBR power plants are planned in the 200 to 500 MWe range to be operational in the 1970's: one by the UK in '72, one by France in '73, one by Germany-Benelux in '75, one in the US in '76 and another one in '78, one in Japan about '78 and two in the USSR, one in '71 and one in '75. In addition a similar number of smaller power plants or test facilities have been built or are planned.

There are GCFR programmes in the US and in Europe supported by manufacturers, by utilities, and by government laboratories but the total annual expenditure directly attributable to this work is probably less than ten million dollars. This figure is however rather misleading as the GCFR relies heavily on the HTGR programmes for helium coolant technology and on the LMFBR programmes for fuel element technology. It is difficult to say how much money is contributed indirectly in this way, and it is not really necessary to specify it as these programmes already exist. The extra research and development work needed to be able to build GCFR power demonstration plants has been estimated as about 250 million dollars over the next ten years, that is between 10 and 15 percent of that planned for LMFBRs.

In the US, studies on helium cooled fast reactors have been in progress for a number of years, mainly by GGA and a group of 42 utilities (representing together 25% of the generating capacity in the US) with support from the AEC. A proposed design for a 300 MWe GCFR power demonstration plant has been prepared and is now being considered by the industry and utilities. This scheme follows the earlier work on larger 1000 MWe power plants which established the feasibility and economic prospects of such plants.

In Europe there have been various national studies on gas cooled fast reactors including those by Belgo-Nucleaire in Belgium, by ASEA-Atom in Sweden, by EIR Würenlingen in Switzerland, by the AEA in the UK and by KFK Karlsruhe in West Germany. In addition there is a study due for completion later in 1970, sponsored by the West German Ministry for Scientific Research, in which KFA Jülich, KFK Karlsruhe, BBK, GHH and Siemens are participating, to assess the potential of the gas cooled fast reactor. Finally the Gas-cooled Breeder Reactor Association was set up in Brussels in October 1969 for an initial period of two years by eleven industrial groups, utilities and government organizations to investigate the technical and economic prospects of helium cooled fast reactors with indirect steam cycle power plants.* This study is also likely to lead to a proposal for a power demonstration plant of around 300 MWe.

* The members of the GBR Association currently are: Belgo-Nucleaire and CEN Mol from Belgium, GAAA and SOCIA from France, Neratoom from the Netherlands, ASEA-Atom from Sweden, BBST from Switzerland, TNPG and CEGB from UK and BBK and GHH from West Germany.

TECHNICAL BACKGROUND

Fast reactors are a difficult technical problem, no matter what coolant is used, as it is essential to get a high breeding gain and a low system inventory of fission material to obtain an adequate doubling time. In the sodium cooled fast reactors there is an inevitable loss of breeding gain compared with that for helium cooled fast reactors because of the neutron moderation and absorption in the sodium coolant. This can be offset by aiming for a high rating and low inventory because of the good heat transfer properties of sodium but the need to minimize the amount of sodium in the core means that small clearances between fuel pins are then inevitable, and this complicates the core design because of possible coolant reactivity effects. With sodium cooling, because of possible chemical reactions and induced radioactivity, it seems essential to have an intermediate sodium circuit between the primary sodium circuit and steam circuit. These problems have led to a major effort being mounted on sodium technology apart from the major effort devoted to research and development on fuel for these reactors.

In a helium cooled reactor the breeding gain is higher and the fuel rating is lower than in a sodium cooled reactor but the doubling time is about the same or even slightly less. The small magnitude of any coolant reactivity effects and hence the wider spacing allowed and needed between fuel pins eases the core design problems considerably. However, high absolute helium pressures are needed in the primary circuit to obtain adequate heat transfer and heat transport rates and the real problem is to maintain adequate helium flow rates in the primary circuit at all times including possible depressurization incidents.

Although the above remarks on sodium cooling and helium cooling are very brief they do bring out the fundamental differences between the two types of cooling. With helium cooling the essential problem is to contain a high pressure helium circuit and to circulate the helium in that circuit at all times. This is essentially an engineering problem which is virtually independent of the reactor, except that the reactor determines the combination of depressurization rates and coolant flow rates which is allowable.

With sodium cooling there is much more interaction between the design of the core and the rest of the circuit because of the reactivity effects of the sodium coolant, and the need to avoid or control reactivity transients arising from phase changes in coolant or dimensional changes in the fuel assembly.

With both coolants, fuel needs to be developed but, provided vented fuel is used with helium cooling, the basic problems are essentially the same and the operating limits can also be the same for pin type fuel. It may be possible with helium cooling to use coated particle type fuel which may be cheaper to fabricate but this is a future development and any realistic comparison made at the present time between sodium and helium cooling must assume pin type fuel with essentially the same operating conditions.

It is perhaps worth noting here that the difficulty is not to design a fast reactor, as this can be done easily by derating the core sufficiently, but to design a safe fast reactor with adequate performance and acceptable cost. The engineering and safety arguments are closely linked together in each type of reactor, and it is not possible to quantify in absolute terms the relative difficulties of LMFBRs and GCFRs but there is no reason to suppose that the design problems cannot be solved in either case.

However, it would seem on the face of it that the GCFR problem is more amenable to solution as it involves essentially matters which are not intimately connected with the core design and which are capable of proving outside a reactor due to the lack of positive reactivity effects from the coolant itself. The larger clearances in the fuel pin assembly in the GCFR mean that irradiation phenomena such as swelling or bowing have less effect on the cooling capability. In other words the GCFR problems seem to be more straightforward engineering ones with far less interdependence on the detailed fuel element design than in the LMFBR case, and involve a coolant technology which is more readily available and needs less research and development effort.

PROPOSALS

Large commitments on sodium technology and LMFBRs were made because at the time this work started it seemed that this was the only route possible to introduce fast reactors. This work is being carried out all over the

world and considerable progress has been made in fuel development and sodium technology. Helium coolant technology was developed for advanced thermal reactors and is being applied first to the high temperature gas cooled reactor. This experience will be directly applicable to the GCFR and the only real extension needed is to higher operating pressures. The initial fuel concept for the GCFR relies almost entirely on the sodium cooled reactor work. The GCFR is therefore not an entirely new reactor concept but a combination of thermal reactor helium technology and sodium fast reactor fuel technology.

At present there is undoubtedly considerable duplication of effort in the several national LMFBR programmes and if there were some rationalization of the various proposed national programmes it would be quite possible to save enough to put the required research and development effort on the GCFR, some 10 to 15% of the total proposed effort on fast reactors. This would make economic sense for everyone as it would ensure production of the information necessary to enable both systems to be judged on their merits, rather than prejudging the result by not obtaining the required information on the alternative system.

International cooperation would seem to be particularly appropriate for the GCFR as by historical accident and by new technical developments this concept arrived on the scene much later than the LMFBR but nevertheless on the basis of present studies looks equally or even more promising and is a real competitor to the LMFBR. As the bulk of the research and development work for each concept is that needed for the fuel and as this is common to each concept it is not as big a change as it looks at first sight to include the GCFR in fast reactor programmes.

As with the LMFBR one of the vital stages is to build one or more power demonstration reactors to establish that the predicted costs are realistic and that the predicted performance can be attained. Here again it is possible to envisage not increasing the number of power demonstration fast reactors already proposed but to make one or two of them helium cooled reactors instead of sodium cooled reactors.

Because of the large amount of government money already invested in seven national sodium cooled fast reactor programmes and because of the smaller amount of money invested by industry there are undoubtedly commercial problems to be resolved to minimize duplication of effort and make money available for helium cooled fast reactors. However, there is an incentive to do this because of the large amounts of money involved and because of the need to obtain information on competitive systems.

Some preliminary meetings were held in Paris in 1968 under the auspices of the European Nuclear Energy Agency (ENEA)* to discuss possible international cooperation on gas cooled fast reactors but no firm proposals emerged although a working group is still in existence and meets once or twice a year to review the position. In addition there are more frequent meetings of specialists working in this field. There has been a definite change in the situation since 1968 and the helium cooled fast reactor now proposed needs less research and development work and this is mainly engineering as the basic fuel concept is now more or less the same as that for the LMFBR.

Since there are now groups both in Europe and the USA actively engaged in helium cooled fast reactor work it would seem appropriate to consider having further meetings under the auspices of ENEA (in which both Japan and the USA are represented as well as the European countries) with the object of coordinating the remaining research and development work on the GCFR and discussing the possibility of constructing one or even two power demonstration plants. Such discussions would need to take into account the commercial realities of the present situation but this should not create insuperable difficulties. It would be to everyone's advantage to have a fast breeder reactor programme rather than an LMFBR programme or a GCFR programme.

* ENEA sponsored the successful OECD High Temperature Reactor Project, the DRAGON Project, in which twelve European countries participated. The DRAGON Project started in 1959 and is still in existence, the total budget up to 1970 being about 75 million dollars.

SUMMARY

1. There is a need for fast reactors in the 1980's provided they can compete economically with the advanced converter reactors available at that time. If they cannot compete then there is no need for fast reactors until later when rising uranium prices will make them competitive.
2. The major effort on fast reactors all over the world is concentrated on sodium cooling, because at the time most of these programmes were started over ten years ago it was the technically appropriate choice.
3. The development of helium cooled reactor technology for HTGRs and the use of prestressed concrete reactor vessels for gas cooled reactors means that helium cooled fast reactors are now technically feasible and must be considered as competitors to sodium cooled fast reactors.
4. Assuming the use of the same basic pin type fuel for each type of fast reactor, then the helium cooled reactor is essentially a problem of the correct design of the high pressure primary circuit with little interdependence on the detailed core design, whilst the sodium cooled reactor design depends much more on the detailed design and behaviour of the core and introduces a more difficult coolant technology.
5. Because of the large amount of money and effort already invested in sodium cooled fast reactors it is difficult to launch a programme for another type of fast reactor on a national basis even though the extra money and effort needed is relatively modest. However, there is a strong case for having a parallel helium cooled fast reactor programme, on an international basis if not on a national basis. There would then be a fast breeder reactor programme rather than an LMFBR or a GCFR programme.
6. There are already studies on helium cooled fast reactors available in the US and there are now studies under way in Europe. These studies seem to confirm that there are no major feasibility questions

remaining, so that a joint effort on an international basis could be considered. Such a joint effort could lead to the construction of a helium cooled power demonstration reactor either in Europe or in the US (or even two reactors) on a time scale comparable to that for sodium cooled power demonstration plants.

7. The money for such a joint international programme could be found either as a moderate additional investment in fast reactor technology or by diverting a modest fraction of the money currently planned to be spent on sodium cooled fast reactors. The incentive for doing this is to make it more certain that there are competitive fast reactors available for commercial exploitation in the 1980's and that the utilities can exercise their normal options of choice as to which reactors they prefer to buy.
8. It is suggested that discussions should take place soon on the desirability of mounting an international programme to develop and prove helium cooled fast reactors so that they can be compared with sodium cooled fast reactors on a similar basis both by industry and by utilities. It is suggested furthermore that such discussions could well take place under the sponsorship of the European Nuclear Energy Agency, which has already taken some initiative in this field.

CLOSING REMARKS

D. B. Trauger, General Chairman, ORNL

If we compare the task of developing the Gas-Cooled Reactor concept to that of climbing a mountain, we now perhaps have come full-around to the side where we started, but at a higher level. When each group entered the helium-cooled reactor field with a serious effort, they first looked at the reactor circuit. The questions were: Could the circuit be designed for the high temperatures and for hot, dry helium at low levels of impurities? Could helium be contained? How would the equipment operate?

Early experiments were reassuring and intensive programs were mounted for the small reactors - Dragon, Peach Bottom, and AVR. Once the reactor projects were under way, it was necessary to fuel them adequately. The HTGR is really successful, as Dr. MacPherson said at the outset of this meeting, because of the coated particle.

Five years ago at Brussels, coated particle fuels which would sustain high temperatures and burnups were reported. Fabrication problems and costs were foremost concerns. By the time of the Jülich meeting in 1968, these problems had been solved and fast neutron damage was just becoming identified as an important problem. It was a threat to particle integrity and, more importantly, to fuel element structures, particularly for bonding.

This week we have heard many papers and discussions on HTGR fuel. It seems safe to say that coated particle fuel and the fuel element structures are now well in hand. We understand the basic performance reasonably well. What may be confusing to the observer and remains to be finished, is the task of finding ultimate limits for service. This includes pushing the technology to reduce costs, to develop thorium recycle methods, and to extend it for direct cycle application.

Thus, for the HTGR, we are at a point where small reactors have worked well, fuel is developed, and large plant designs have been produced with prestressed concrete pressure vessels. Fort St. Vrain construction experience indicates that the path may be correct.

Now, in coming around the mountain, we fortunately have climbed to the new level, one from which the vista of a fertile plain for commercial application seems to be clearly visible.

The road still leads upward, however, and I think we are back to the point where more effort must be put on the circuit. This is obvious for the commercial designs where equipment must be highly reliable. However, the advanced tasks to be achieved require better materials, new devices and equipment, perhaps even new materials. Roy Huddle helped to point the way in times past on Magnox cans and coated particles. I note that he now works on the behavior of materials at higher temperatures than those presently needed. This may bear watching and has been emphasized in the papers today.

It has been brought out in these four days that there is substantial incentive to develop the direct cycle and particularly the Gas-Cooled Fast Breeder. The GCFR may be vitally important to an optimum fulfillment of long-range energy needs.

It also points to further international cooperation such as that suggested by Mr. Rennie and as that exhibited here this week. I thrill as greatly to this exemplification of what can be done through the cooperation of people from many places as to the progress in Gas-Cooled Reactor technology. We must do all that is possible to further both causes. There seems to be a unique opportunity for us to provide some light in troubled times.

Thank you one and all - good evening.

Pages 919-930 excluded

PARTICIPATING ORGANIZATIONS

BELGIUM

Centre d' Etude de l' Energie Nucleaire (CEN)

European Atomic Energy Community (EURATOM)

Gas-Cooled Breeder Reactor Association (GCBRA)

Société Belge Pour l' Industrie Nucleaire, S. A. (Belgonucleaire)

BRAZIL

Central Eletrica de Furnas S.A., (FURNAS)

Comissao Nacional de Energia Nuclear (CNEN-C)

CANADA

Atomic Energy of Canada, Ltd. (AECL)

FRANCE

Commissariat a l' Energie Atomique (CEA)

European Nuclear Energy Agency (ENEA)

GERMANY

Arbeitsgemeinschaft Versuchsreaktor GmbH (AVR)

Brown, Boveri & Cie., AG (BBC)

Brown Boveri/Krupp Reaktorbau GmbH (BBK)

Bundesanstalt für Materialprüfung (BFM)

Gesellschaft für Kernforschung mbH (GfK)

Gutehoffnungshütte Sterkrade AG (GHH)

Kernforschungsanlage Jülich GmbH (KFA)

Kernforschungszentrum Karlsruhe (KfK)

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Central Electric Generating Board (CEGB)

OECD High Temperature Reactor Project (Dragon Project)

Private Consultant - Compton A. Rennie

The Nuclear Power Group, Ltd. (TNPG)

United Kingdom Atomic Energy Authority - Reactor Group (Risley)

UNITED STATES OF AMERICA

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Argonne National Laboratory (ANL)

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Texas Electric Service Company
Union Carbide Corporation

Union Electric Company

United Nuclear Corporation

United States Atomic Energy Commission (USAEC)

United States Department of Interior

University of Illinois (Univ. of Ill.)

Westinghouse Corporation

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ERRATA FOR REPORT CONF-700401

The corrections listed below should be made in the subject report. In addition, enclosed is a revised author index to be substituted for the one included in the original copy.

Page 315, Table 1, inventory of $^{134}\text{Caesium}$ should be 4.4×10^6 .

Page 345, fifth line from top of page should read:
"With only 150°C difference between A and B"