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# SYSTEMS-DESIGN AND ENERGY-BALANCE CONSIDERATIONS FOR IMPACT FUSION

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## I. INTRODUCTION

The approach to thermonuclear fusion power embodied in the term "impact fusion" envisages the acceleration of a DT-bearing projectile to velocities in the range $10^5$ - $10^6$ m/s and a subsequent impact with a stationary target or a similarly accelerated projectile. Heating to and burn at thermonuclear temperatures would be achieved by means of a coupled shock-heating and adiabatic compression process. No magnetic fields would be present, and the dominant energy losses would occur through radiative and thermal conduction channels. "Bootstrap" heating by alpha-particle deposition into the DT plasma under certain conditions may be possible. Conceptual designs and rudimentary systems studies of power reactor embodiments based on the impact fusion approach are for all intents and purposes nonexistent. Furthermore, the relationship between projectile velocity and thermonuclear yield have been estimated only by approximate models and analyses. The focus of these analyses[1,2] has been the elucidation of the relationship between projectile velocity and temperature upon impact; accurate energy balances yielding useful projectile gain <u>versus</u> input energy simply do not exist.

In view of the durth of system design studies and fundamental calculations of projectile yield, a paper of this nature can only rely on the results, insight and indications generated by more comprehensive studies of other fusion concepts. Additionally, simple scoping calculations can be made of limiting and sometimes unrealistic situations in order to bracket the expected projectile gain and input energy requirements. Without even highly approximate estimates of the gain <u>versus</u> yield relationship, any prognosis of reactor viability will be almost meaningless.

Because of the absence of substantive experience, design studies, and theoretical physics analysis, the posture of this study is highly qualitative and approximate. The primary intent is to point out areas of concern and potential problems within an overall systems context, rather than to present a polished and optimized Impact Fusion Reactor (IFR) design. After a parametric and qualitative description of the general energy-balance and systems considerations in Sec. II, Sec. III addresses a number of reactor design problems anticipated for the IFR. Section IV attempts to approximate and/or to define the operating regime for an IFR based on highly simplified but limiting projectile/target energy balances and thermonuclear burn models. Major conclusions and/or indications are summarized in Sec. V.

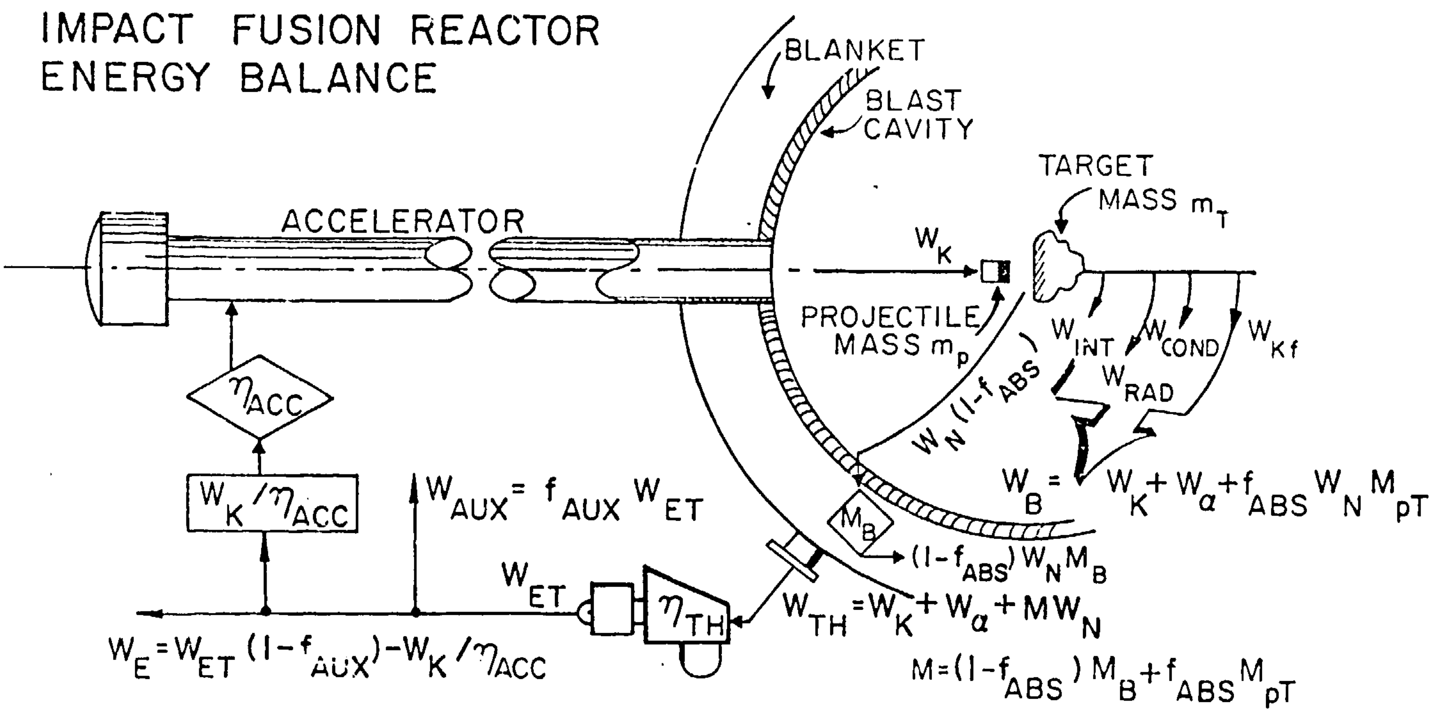
## II. GENERAL ENERGY-BALANCE AND SYSTEMS CONSIDERATIONS

The essential elements of the IFR are depicted schematically on Fig. 1 in terms of a generalized energy balance. These elements include:

- A macroparticle accelerator with the capability of imparting a kinetic energy $W_K$ with an overall efficiency $\eta_{ACC}$ to a DT projectile.
- A projectile transport and guidance system that is capable of accurate and rapid injection of projectiles into a reactor or target chamber.
- An energy store and power supply for the projectile accelerator.
- A system for rapid replacement of targets and auxiliary equipment destroyed after each implosion.
- A reactor or target chamber that is surrounded by a medium for blast or shock attenuation and/or absorption. Generally, this chamber is defined by the boundaries of a blast cavity, outside of which all structures must function with an acceptably long life-time.
- A blanket system that provides a multifunction region where tritium is bred (only a DT fusion reaction is considered), and where moderation of the 14.1-MeV fusion neutron, heat removal, and radiation shielding occur. A portion of these functions may be performed by materals placed within the blast cavity.[3]
- A means to extract the thermal power received by and deposited into the blanket system. The thermal power must be steady state, must be delivered with less than ~ 5-10 K temperature fluctuation, and could be used to generate either electricity (as shown in Fig. 1) or process heat for synthetic fuel production.[4,5]
- A turbine-generator energy-storage and switch-yard system that as a minimum must be capable of generating and distributing all electrical energy used within the power plant without large power surges, while simultaneously assuring a source of constant and reliable electrical power to a user.
- An auxiliary support system needed to sustain and to maintain the operation of the IFR power station on an > 80% basis. For example:

- tritium recovery, purification and processing from the breeding blanket.
- fabrication and recycle of projectile, target, and destroyed ancillary equipment within the blast cavity.
- remote maintenance and repair systems
- control and instrumentation systems, particulary as applied to the synchronous operation of projectile/target acceleration, guidance, and abort (if necessary) functions.

Each of these major subsystems must function at an acceptable level of reliability and cost, while simultaneously operating as an integrated system to render a favorable net energy balance that is compatible with as yet proven or resolved physics and engineering technology issues. The engineering energy balance depicted on Fig. 1 can be evaluated in terms of a projectile gain, $Q = (MW_N + W_\alpha)/W_K = W_F/W_K$, that is define in terms of the primary 14.1-MeV




$$Q = \frac{MW_N + W_\alpha}{W_K}$$

$$Q_E = W_{ET}/W_C = \frac{\eta_{ACC}\,\eta_{TH}(1+Q)}{1 + f_{AUX}\,\eta_{ACC}\,\eta_{TH}(1+Q)}$$

Fig. 1 Generalized energy-flow diagram for a conceptual Impact Fusion Reactor (IFR).

neutron yield, $W_N$, multiplied by M to reflect exoergic nuclear reactions occuring within the blanket, the alpha-particle yield, $W_\alpha$, and the initial projectile energy, $W_K$. As a measure of overall plant performance, an engineering Q-value, $Q_E = W_{ET}/W_C$, is defined as the ratio of total electrical energy generated from each implosion, $W_{ET}$, relative to the total recirculating energy requirement, $W_C = f_{AUX}W_{ET} + W_K/\eta_{ACC}$, where $f_{AUX}$ represents the fraction of $W_{ET}$ needed to drive all auxiliary plant power requirements (feedwater pumps, "housekeeping" power, etc., $f_{AUX} \sim 0.05$, typically), and $W_K/\eta_{ACC}$ is the energy demanded by the projectile accelerator. The following expression relates $Q_E$ to Q:

$$Q_E = \frac{\eta_{ACC}\eta_{TH}(1+Q)}{1 + f_{AUX}\eta_{ACC}\eta_{TH}(1+Q)} \quad , \tag{1}$$

where $\eta_{TH} \simeq 0.3$-$0.4$ is the thermal to electric conversion efficiency. Equation (1) is displayed on Fig. 2 parametrically in $\eta_{ACC}$, the projectile accelerator efficiency. Since recirculating power fractions, $\varepsilon = 1/Q_E$, below $\sim 0.15$-$0.20$ are desirable for economic reasons,[6] a $Q_E$ in the range 5-6 would require projectile gains, Q, in the range 40-50 if the accelerator efficiency can be maintained in the range 0.6-0.4. It is noted that a "coupling coefficient" that gives the fraction of the incident energy, $W_K$, which actually appears as increased internal energy of the DT is embedded in the parameter Q. The coupling coefficient is highly dependent on the projectile/target design and is not introduced at this level of analysis. The projectile velocity, u, and energy, $W_K$, needed to achieve desirable gains are simply not accurately known today for impact fusion. Section IV attempts to establish bounds on this crucial relationship between Q and $W_K$, (i.e., the so-called "gain curve"). This Q versus $W_K$ relationship is vitally important for technological reasons, as well for the plant energy balance and system economics. As indicated on Fig. 1, the energy $W_B = W_K + W_\alpha + f_{ABS}W_N M_{pT}$ can potentially contribute to a significant blast or shock containment problem. In addition to $W_\alpha$ and $W_K$, the fraction $f_{ABS}$ of the 14.1-MeV fusion neutrons can be absorbed by and multiplied in energy ($M_{pT}$) through nuclear interactions with the destroyed projectile and target/support structure; if the associated masses, $m_p$ and $m_T$, are sufficient, $f_{ABS}$ may be as large as 0.1-0.2.[3] Consequently, even for high-gain

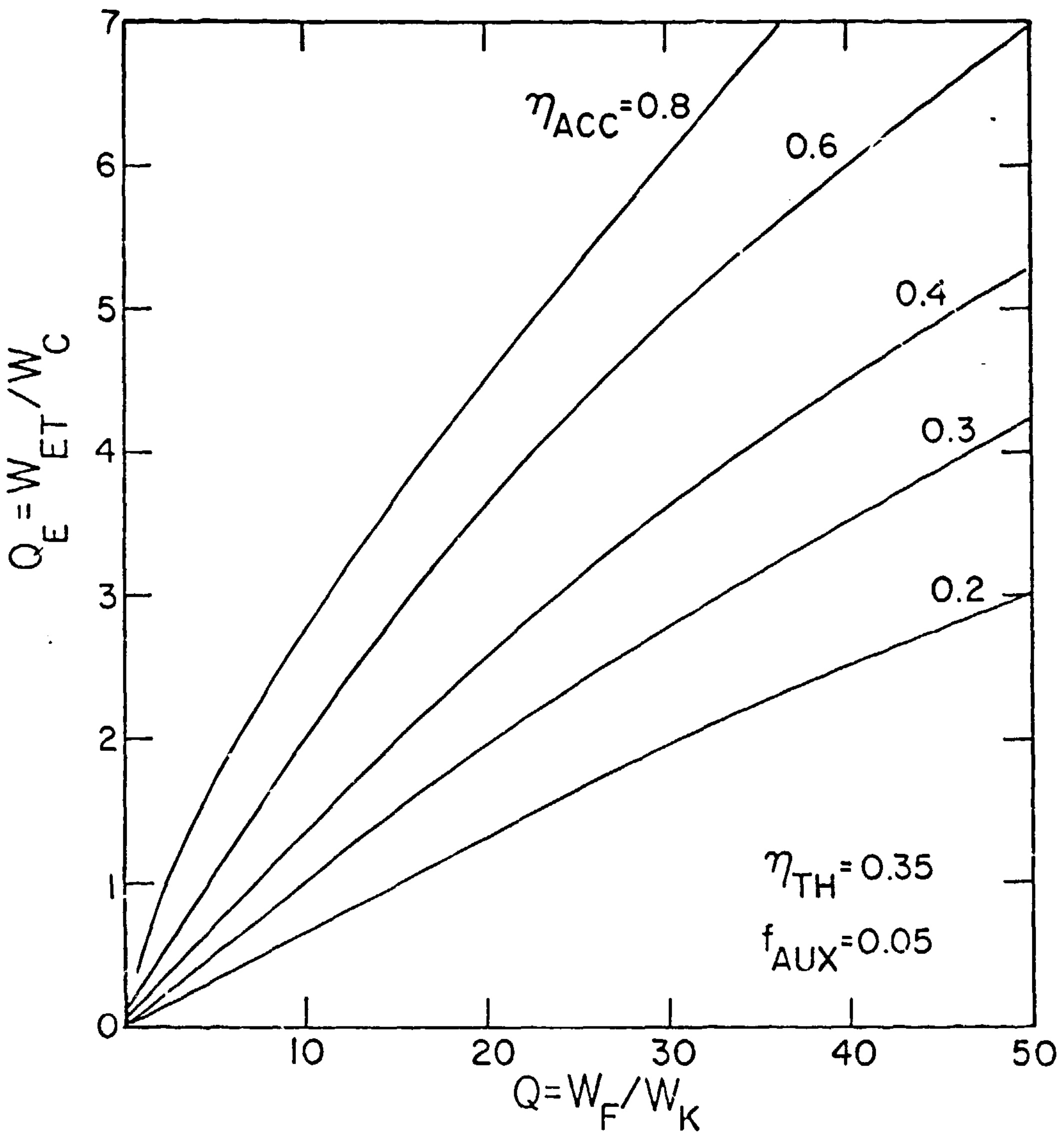



Fig. 2 Parametric systems design curves for an Impact Fusion Reactor (IFR).

projectile/target systems ($W_F = MW_N + W_\alpha >> W_K$), as much as 30-40% of the fusion yield can appear as structurally destructive blast energy, $W_B$. The severity of this problem depends crucially on the amount of mass ($m_p + m_T$) accelerated during an implosion, the magnitude of $W_B$, and, obviously, the Q <u>versus</u> $W_K$ relationship.

## III. REACTOR SYSTEMS DESIGN CONSIDERATIONS

The extrapolation of most fusion confinement schemes to reactors must be accompanied by a complex interaction between physics, engineering and electric utility constraints. Ultimately, a proposed power system should promise safe, reliable, and economic operation, as evaluated at the time of its implementation. The accuracy of such projections depends sensitively upon the existing theoretical and experimental physics base. Figure 3 presents diagramatically the major physics/engineering/utility interfaces expected for a power system based on the impact fusion scheme. Within each discipline perceived issues and/or problems are grouped according to functional subsystems. For example, the complex interaction between projectile/target phenomena, the physics basis for a gain curve (Q versus $W_K$), and the technology implications of the magnitude and form of the blast energy, $W_B$, have been discussed in Sec. II. Additionally, the pellet/target mass and the extent of auxiliary support structure damage could be reflected as a significant operating cost.[3] For example, a 1-GJ thermonuclear yield with $\eta_{TH} = 0.35$ and $Q_E = 5$ (Fig. 1) corresponds to a net electrical energy of 78 kWeh, which at 50 mills/kWeh would yield a net revenue of \$3.90; given that at most 20% of this revenue can be appropriated towards projectile/target replacement costs, these costs cannot exceed \$0.78 per implosion. It remains to be seen if this cost constraint can be met or if economic considerations will dictate larger thermonuclear yields.

Similar to the scaling of projectile/target costs with thermonuclear yield, the cost of the blast cavity and containment vessel must be carefully analyzed. These latter cost will fall into the category of capital investment and, unlike the operational costs of projectile/target replacement, may show an optimum with thermonuclear yield.[7]

A number of key physics and technology "drivers" can be identified for impact fusion, in addition to the issues of projectile/target and blast cavity costs described above. Although more detailed studies of other inertial fusion schemes can lend valuable insight into these systems problems/uncertainties, eventually device-specific analysis of an impact-fusion reactor embodiment will have to be performed if an unambiguous physics/technology assessment is to result. This kind of in-depth analysis, however, should not be performed until a reasonable operating point(s) can be identified (i.e., a promise of economic fusion gain at an acceptable yield and energy input). Given that a favorable, realistic energy balance can be developed that is based on a credible estimate of fusion yield for a specific projectile/target configuration, the following systems issues should be subjected to detailed analyses:

- Identify type, size, efficiency and cost of a high pulse-rate, macroparticle accelerator. Clearly, this crucial component of the impact fusion system should be examined in parallel with the physics of the projectile/target interaction and the realistic estimate of the Q versus $W_K$ gain curve.

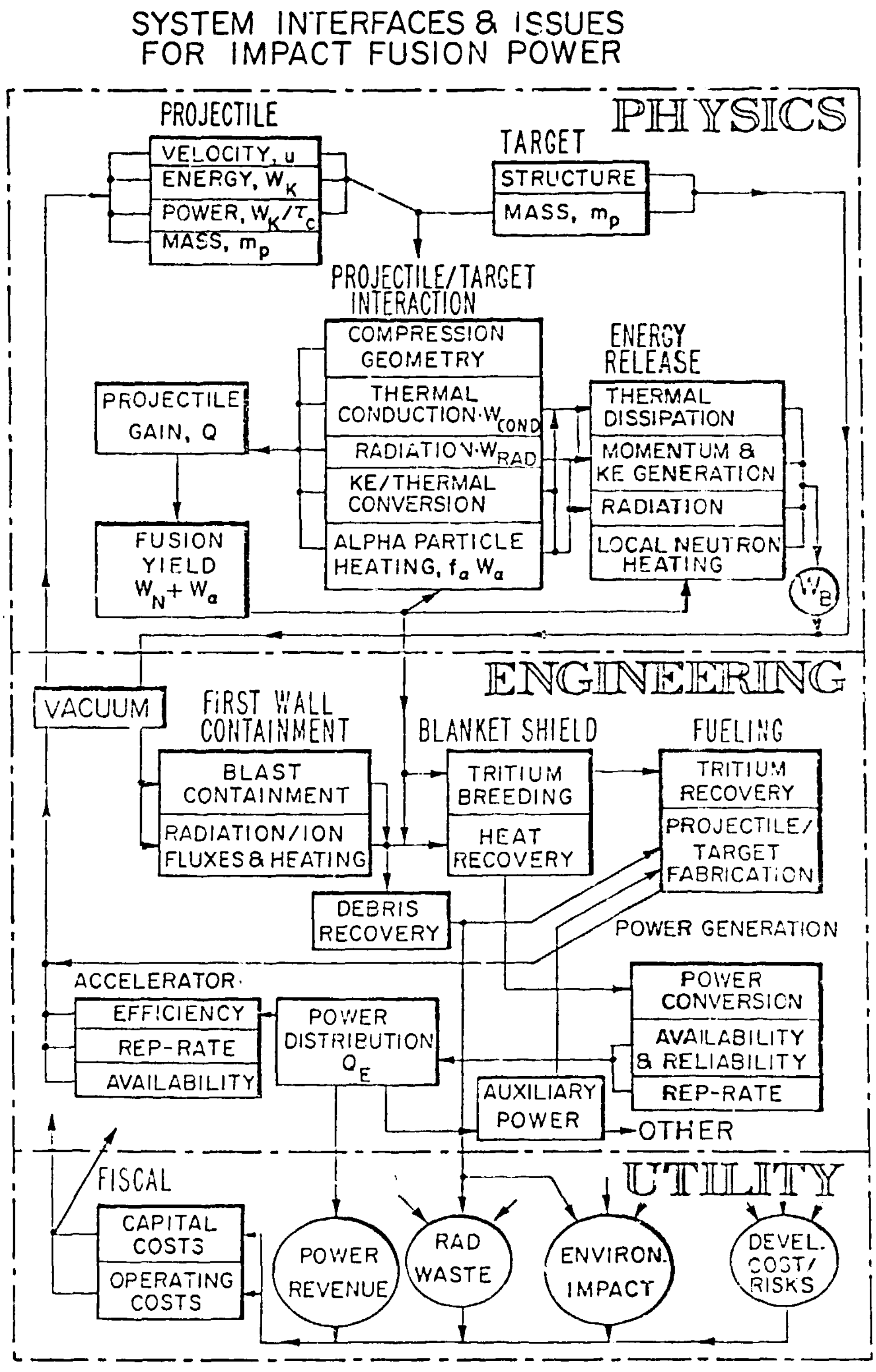



Fig. 3 Systems interfaces for an Impact Fusion Reactor (IFR).

- Systems design considerations for the reactor core and nuclear island include:

  - projectile transport, guidance, and entry system. A discardable, replacable vacuum barrier or a quick-acting "gate" situated at the accelerator/blast-cavity interface may be required.

  - the mechanism by which the target and destructible structure is inserted, replaced, and recycled must be resolved, unless a companion accelerator and projectile is used in place of a target.

  - the structural loads caused by blast-related momentum transfer and the means by which these loads can be attenuated (if necessary) must be resolved. Can a lithium spray be employed as a blast attenuator, tritium breeder, and coolant?[3]

  - the design of the first permanent structural wall represents a crucial issue for this pulsed power source. An appreciable fraction of the thermonuclear yield in all likelihood will pass through this structure as thermally conducted heat, and the lifetime of this cavity wall could represent a major technology/cost driver. What are the consequences and means to deal with a projectile/target or projectile/projectile trajectory mismatch?

  - Although an IFR will operate in a highly pulsed mode, the thermal-hydraulic systems (blanket, coolant, etc.) must function in a thermal steady state. Other blanket design considerations (i.e., tritium breeding, shielding, etc.) are expected to differ little from those proposed for other fusion concepts.

- A large number of ex-reactor issues can be identified, aside from the projectile accelerator and its system requirements.

  - What is the relationship between the projected yield curve, accelerator and blast cavity pulse rate, total power, blanket response, and system economics/costs? For instance, a 1-B$ accelerator that drives a 1-GJ yield with $\eta_{TH} = 0.35$ and $Q_E = 5$ will have to be pulsed at 5 Hz in order to maintain the accelerator capital cost for this 1400 MWe(net) plant below 700 $/kWe, or ~ 30% of the anticipated goal for total plant investment.

  - As noted previously, the operating cost associated with projectile/target fabrication and recycle could consume a measureable fraction of the plant revenue. The tradeoffs between this technology/economics issue and the physics-dictated projectile/target design must be resolved. The related issue of radwaste associated with projectile/target debris may also be important.

  - The degree of thermal cycling of the primary coolant exiting the reactor blanket must be minimized to 5-10 K.

  - The degree of cavity modularization needed to defray the cost of a potentially expensive accelerator, by more effectively using this investment, may play an important economic role.

- Protection of the capital investment against "stray" projectile trajectories in a high repetition-rate system may prove to be important.

- A majority of fusion power schemes tend to operate with large size, power output, and total capital outlay. Does the impact fusion scheme differ in this respect by offering a potentially small but economic system?

- The feasibility of designing and operating reliable and economic subsystems should be addressed.

- The issue of plant availability is directly related to the ease of remote maintenance and the facility for rapid changeout/replacement/repair of key system components.

As noted previously a detailed assessment of many of these issues is not warranted until a better understanding is developed of the relationship between accelerator requirements, projectile/target design, and the thermonuclear yield/gain relationship. The following section addresses these questions by means of a highly-simplified, analytic model.

## IV. APPROXIMATE AND LIMITING ENERGY BALANCES

In order to assess, at a preliminary level, the reactor viability of the impact fusion approach, the relationship between initial projectile velocity, u, total thermonuclear yield $W_F = (MW_N + W_\alpha)$, and the ratio, $Q = W_F/W_K$, of the thermonuclear yield to the initial projectile energy is needed. An analytic or numerical determination of the inter-relationship between u, $W_F$, and Q is made difficult by the multidimensional and coupled nature of this hydrodynamic, shock and radiation-transfer problem. Consequently, calculations and modelling of the kind represented by Refs. 1 and 2 have been primarily concerned with estimating the relationship between final temperature and initial projectile velocity in the presence of classical loss processes. A self-consistent resolution of the trade-offs and limitations of thermonuclear yield, as embodied in Q or $W_F$, is rarely given because of the approximate and simplified nature of the analytic models. Unfortunately, even the most approximate assessment of an IFR cannot be made without even a simplified yield curve (i.e., relationship between Q and u, $W_K$ or $W_F$).

Any inhibition associated with avoiding the presentation of definite Q-values expected for an IFR because of the poorness and/or limitations of the phenonological model is cast aside here. The simple shock-heating model reported in Ref. 1 is used to estimate Q for a one-dimensional (planar) impact without adiabatic compression. The constraints imposed by classical thermal conduction and bremsstrahlung radiation are examined. Although no claim is made as to the exactness of the results that emerge from this simple analysis, these results do serve as a reference from which the degree to which improvement in device performance from multidimensional effects, adiabatic compression and alpha-particle heating can be qualitatively estimated. Generally, the predictions of this simple shock model are expected to be pessimistically conservative. The improvement expected by compressional heating of a shocked planar DT medium is examined subsequently.

## A. DESCRIPTION OF IDEAL SHOCK MODEL*

A cylindrical DT projectile of initial length L, density $\rho_o$, and radius $R \simeq L$ is assumed to impact axially a perfectly inelastic barrier at a velocity u. An ideally sharp shock is postulated to move in one dimension through the projectile at a velocity $v_s$ relative to the projectile or velocity $\dot{z}$ relative to the barrier (laboratory frame). Dimensional changes in the radial direction are ignored. Figure 4 depicts this model schematically. The Hugoniot relationships are used to determine the shock conditions, which are then applied to estimate the thermonuclear yield and loss rates. Referring to Fig. 4, the Hugoniot relationships are

$$u = \frac{2}{\gamma+1} v_s \tag{2A}$$

$$\rho_s = \frac{\gamma+1}{\gamma-1} \rho_o \tag{2B}$$

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* Except for plasma temperature, $T_e = T_i = T(\mathrm{keV})$, mks units are consistently used. The electronic charge, $e = 1.60(10)^{-19}$ J/eV is used to represent the Boltzmann constant $k_B$(i.e., $10^3 e\, T(\mathrm{keV}) = k_B T(\mathrm{K})$). Other constants used are: fusion energy release, $E_N$ = 20 MeV/fusion; mass of a proton, $m_p = 1.67(10)^{-27}$ kg; heat capacity ratio $\gamma$ = 5/3; atomic mass unit for DT, A = 2.5; initial DT density, $\rho_o$ = 200 kg/m$^3$; Coulomb logarithim, $\ell n\Lambda$ = 10.

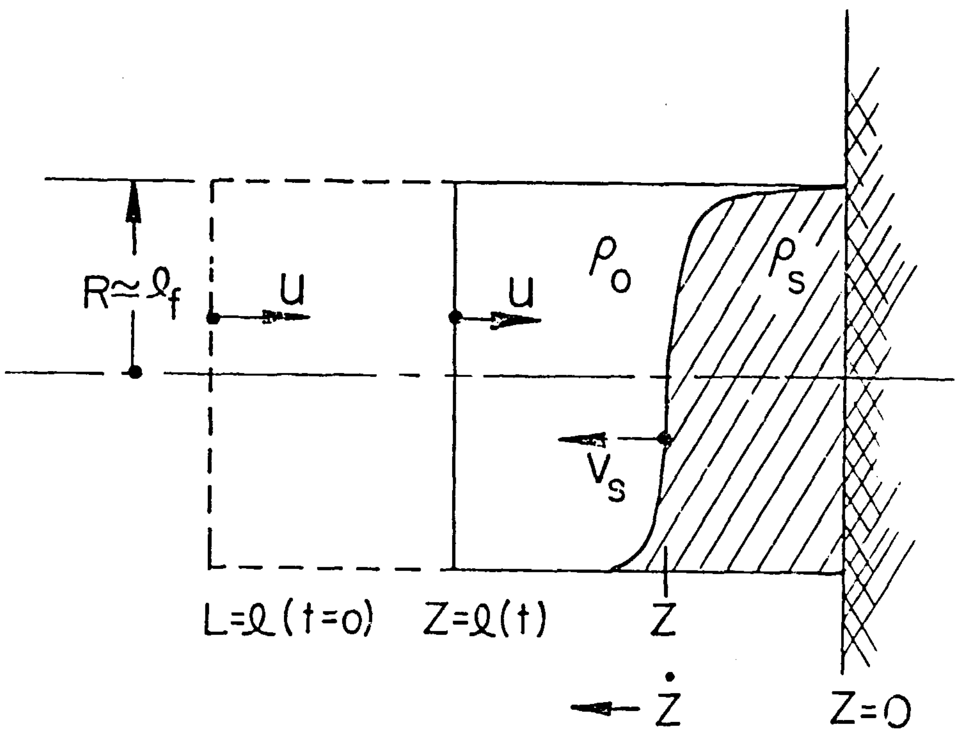


Fig. 4 Schematic diagram of one-dimensional shock-heated projectile model without adiabatic compression.

$$P_s = \frac{2}{\gamma+1}\rho_o v_s^2 = \left[\frac{2(10)^3 e}{Am_p}\right]\rho_s T \tag{2C}$$

$$\dot{z} = \frac{\gamma-1}{2} u = \frac{\gamma-1}{\gamma+1} v_s \quad . \tag{2D}$$

The thermonuclear fusion yield $W_F(J)$ is given by

$$W_F = \int_o^{\tau_B} E_N \left(\frac{1}{4} n_s^2 <\sigma v>\right)\pi R^2 z dt \tag{3A}$$

$$W_F/\pi R^2 = \frac{E_N(10)^6 e}{8(Am_p)^2}\left(\frac{\gamma-1}{\gamma+1}\right)\left(\frac{<\sigma v>}{v_s}\right)(\rho_s \ell)^2 \ , \tag{3B}$$

where the burn time $\tau_B$ has been taken as one shock transit time, $L/v_s$, $\rho_o L = \rho_s \ell$ by mass conservation and $n_s = \rho_s/Am_p$. Given that the initial kinetic energy of the projectile, $W_K/\pi R^2 = \rho_o L u^2/2$, the following expression for $Q = W_F/W_K$ results

$$\begin{aligned} Q &= \frac{10^{3/2} E_N}{16(Am_p e)^{1/2}} (\gamma-1)^{1/2} \frac{<\sigma v>}{T^{3/2}} (\rho_o L) \\ &= 1.25(10)^{24} \frac{<\sigma v>}{T^{3/2}} (\rho_o L) \ . \end{aligned} \tag{4}$$

For example, at $T = 10$ keV, $Q$ equals $4.30(\rho_o L)$. The projectile energy, $W_K (J/m^2)$, and velocity, $u(m/s)$, are given by

$$W_K/\pi R^2 = 1.\ 5(10)^{11} T\ (\rho_o L) \tag{5}$$

$$u = 4.80(10)^5 T^{1/2} \tag{6}$$

If the classical electron thermal conductivity, $k$(W/m keV), is taken as[8]

$$k = \frac{9.8(10)^{14}}{\ell n \Lambda} T^{5/2} \ , \tag{7}$$

and with thermal conduction power loss per unit volume of an equivalent sphere of radius $R \sim \ell$ equal to $3kT/\ell^2$, the thermal conduction time, $\tau_{COND} \cong 3(10^3 enT)/(3kT/\ell^2)$, equals

$$\tau_{COND} = \left[\frac{10^3 e \ell n \Lambda}{9.8(10)^{14} Am_p}\right] \frac{\rho_s \ell^2}{T^{5/2}}$$

$$= 3.84(10)^{-4} \rho_s \ell^2 / T^{5/2} \quad . \tag{8}$$

Equating $\tau_{COND}$ to the effective burn time, $\tau_B \simeq L/v_s$, gives the following expresion for $\rho_o L = \rho_s \ell$

$$(\rho_o L)_{COND} = \frac{0.15}{\ell n \Lambda} T^2 \quad . \tag{9}$$

The volumetric bremsstrahlung power loss is approximated by[9] $5.35(10)^{-37}\, n^2 T^{1/2}$ ($W/m^3$), which when divided into the plasma energy, $3(10)^3 enT$, gives the following expression for an effective time constant for radiation loss

$$\tau_{BR} = \left[\frac{3(10)^3 eAm_p}{5.35(10)^{-37}}\right] \frac{\gamma-1}{\gamma+1} \frac{T^{1/2}}{\rho_o}$$

$$= 9.37(10)^{-7}\, T^{1/2}/\rho_o \quad . \tag{10}$$

Again, equating $\tau_{BR}$ to $\tau_B \simeq L/v_s$ gives the following expression for a $\rho_o L = \rho_s \ell$ related to radiation losses

$$(\rho_o L)_{BR} = 2.40T \tag{11}$$

B. EVALUATION OF SIMPLE SHOCK MODEL

Equations (4), (9) and (11) for Q, $(\rho_o L)_{COND}$, and $(\rho_o L)_{BR}$, respectively, are plotted in Fig. 5 in the form of $\rho_o L$ versus T. Also shown for convenience is Eq. (6), giving the relationship between u and T. For $\rho_o L$ values below the $(\rho_o L)_{BR}$ curve on Fig. 5, $\tau_B$ is less than $\tau_{BR}$ and a proper kinetic analysis would predict a burn temperature that is relatively uneffected by radiation losses over a period equal approximately to the burn time. Similarly, for $\rho_o L$ values above the $(\rho_o L)_{COND}$ curve on Fig. 5, $\tau_B$ is less than $\tau_{COND}$, and again a region is defined where conduction losses should not be serious. The wedge-shaped region on Fig. 5, where $(\rho_o L)_{COND} < \rho_o L < (\rho_o L)_{BR}$, indicates conditions where both radiation and conduction losses might occur without seriously degrading the

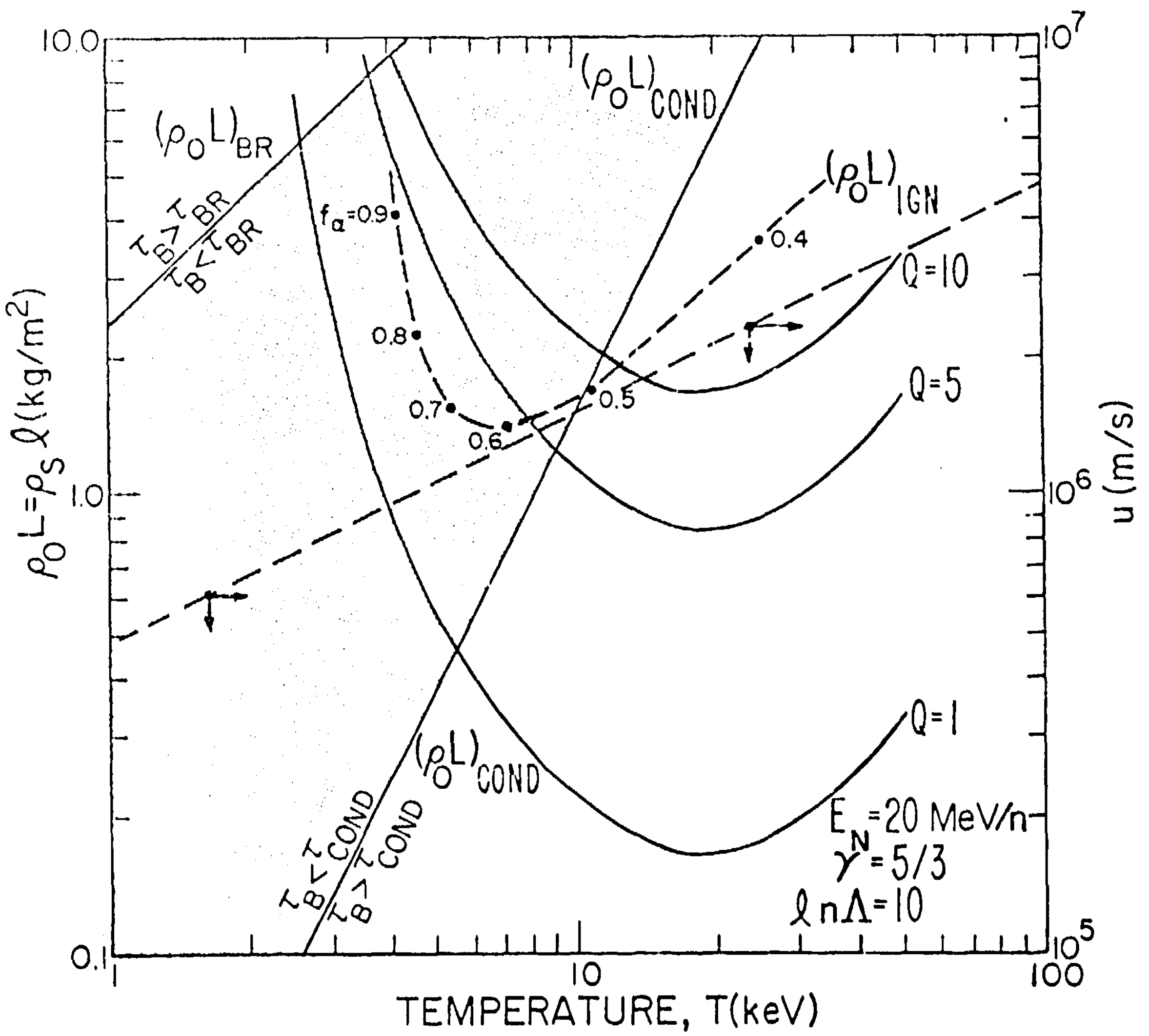


Figure 5. Temperature dependence of $\rho_o L$ for various constraints.

defined burn (shock) kinetics. The parametrically evaluated Q curves on Fig. 5 (Eq. (4)) indicate that radiation and/or conduction would limit Q to values below ~ 8 for this purely shock-heated example.

The results presented in Fig. 5 indicate regions where radiation and/or conduction losses may represent significant and voracious sinks for the ideally transformed projectile kinetic energy. Clearly, these energy sinks would most desirably be supplied by the fusion process itself, i.e., alpha-particle heating. Before the DT alpha-particle reaction product can deposit an appreciable fraction of the 3.5-MeV alpha-particle energy, the thermalization range $\lambda_\alpha$ must approach the heated projectile dimension, $\ell$. The alpha-particle range, $\lambda_\alpha(m)$, is given by[10]

$$\lambda_\alpha = 1.38(10)^{26} \frac{T^{3/2} E_\alpha^{\ 1/2}}{n \ell n(1/\delta)} \quad , \tag{12}$$

where

$$\begin{aligned} \delta &= (1 + m_e/m_\alpha) e n^{1/2}/T^{3/2} \\ &= 1.73(10)^{-18} n^{1/2}/T^{3/2} \quad , \end{aligned} \tag{13}$$

and alpha-particle thermalization on electrons has been assumed to dominate. The quanitity $f_\alpha$ is defined as the fraction of the 3.5-MeV alpha-particle energy, $E_\alpha$, deposited into a heated projectile of average dimension $<\ell>$. This average dimension is defined as four times the volume-to-surface ratio (i.e., $<\ell>$ is a "wetted perimeter" and equals $2\ell$ for a slab of thickness $\ell$ or a cylinder of radius $\ell$, or $4\ell/3$ for a sphere of radius $\ell$). Following the usual transport approximation, $f_\alpha$ is given by

$$f_\alpha = 1 - 1/(1 + <\ell>/\lambda_\alpha) \quad . \tag{14}$$

In the limit $<\ell>/\lambda_\alpha >> 1$, therefore, $f_\alpha$ approaches unity and good alpha-particle confinement results. In the opposite extreme, $<\ell>/\lambda_\alpha << a$, $f_\alpha$ approaches zero and the potential for "bootstrap" self-heating is nil. For a homogeneous projectile, perfect alpha-particle energy confinement ($f_\alpha = 1$) is not possible since some alphas will always be born within a mean free path length of the surface and will escape prior to thermalization. Substituting Eqs. (12) and (13) into Eq. (14) gives the following relationship between $\rho_o L$ and $f_\alpha$ for a homogeneous projectile

$$(\rho_o L)_\alpha = \frac{0.54\ T^{3/2}}{\ell n(1/\delta)} \left(\frac{f_\alpha}{1-f_\alpha}\right) \quad . \tag{15}$$

Figure 6 gives the dependence of $(\rho_o L)_\alpha$ on T for a range of specified alpha-particle energy trapping efficiencies, $f_\alpha$. Shown also on Fig. 6 are the loci of points where the alpha-particle power deposited within the projectile, $f_\alpha P_\alpha$, equals the radiation power, as well as the radiation plus conduction powers. The latter curve represents the locus of ignition points, and the corresponding values of $(\rho_o L)_{IGN}$ are also included on Fig. 5. The achieveable Q-values, as predicted by this simple, one-dimensional shock-heated model, are unacceptably low from the viewpoint of an engineering power balance.

If longer burn times and, consequently, higher Q-values are to be achieved, the system must be designed for and operate with significant alpha-particle heating in order to maintain a thermonuclear plasma against classical radiation and conduction losses. The increase in Q accompaning a burn time that is sustained for considerably more than a single shock transit time, however, can be determined only by a kinetic model of the ignited system. The results of this analysis, as presented on Fig. 5, indicate a high potential for an ignited mode of operation. Furthermore, the density compression that accompanies a purely shock heating is very low ($\rho_s/\rho_o = 4$, Eq.(2B)), and the large dimensions required to give a $\rho_o L$ with a sufficient Q (Eq.(4)) translate into considerable input energies, $W_K$, and thermonuclear yields. This situation is best shown quantitatively by combining Eqs. (4) and (5) to give

$$Q = 1.09(10)^{13}\left(\frac{<\sigma v>}{T^{5/2}}\right)\left(\frac{W_K}{\pi R^2}\right) \quad . \tag{16}$$

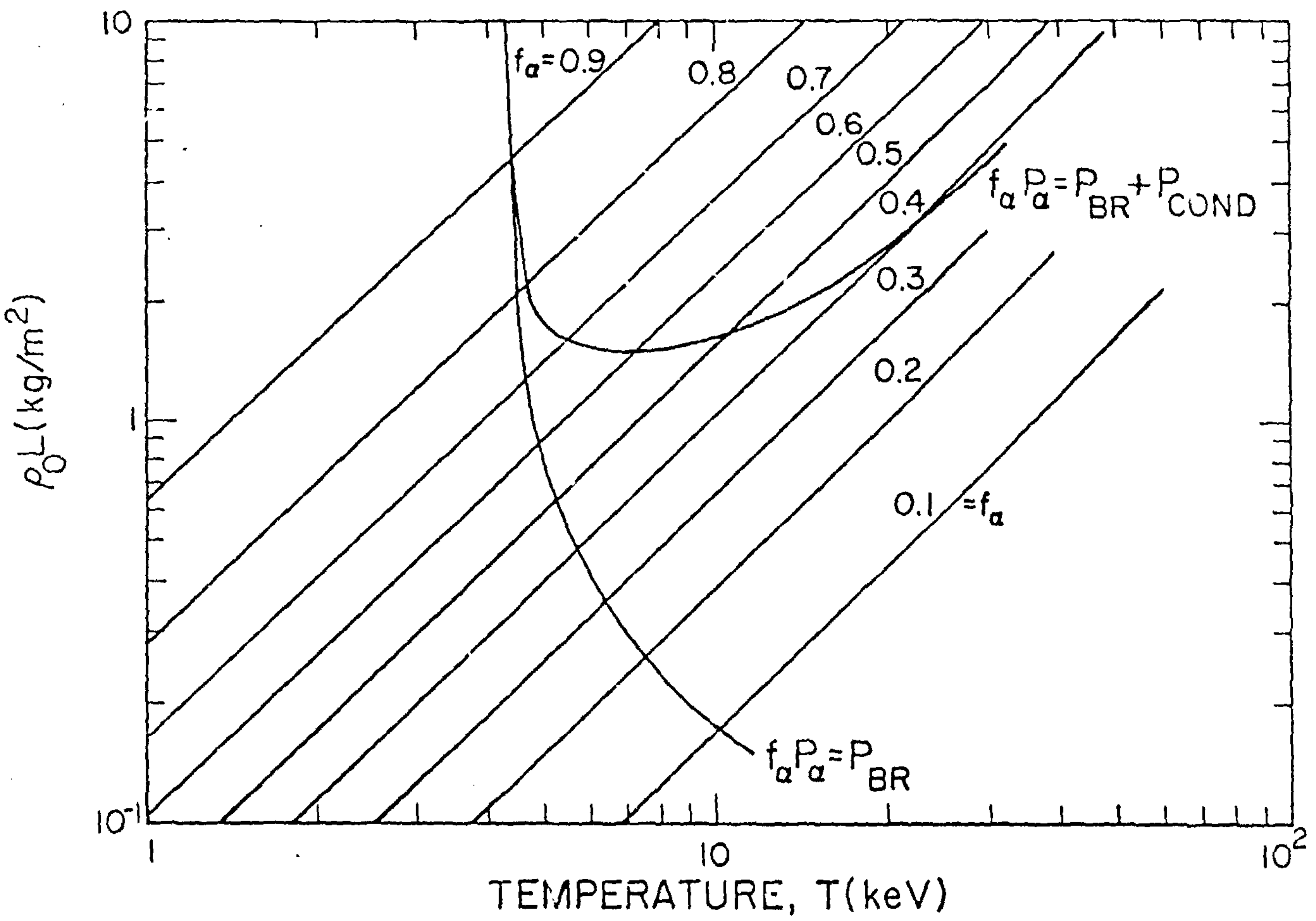


Figure 6. Temperature dependence of $\rho_o L$ required to trap a fraction $f_\alpha$ of the 3.5-MeV alpha-particle energy.

In order to obtain an explicit relationship between Q and $W_K$ (i.e., the yield curve) for this shock-heated case, the projectile radius, R, is taken equal to the compressed length $\ell$ (near minimum surface-to-volume ratio at full compression), and $\rho_o$ is equated to the density of cryogenic DT (~ 200 kg/m$^3$).

For $T = 10$ keV ($\langle\sigma v\rangle/T^{5/2}$ is fairly insensitive to temperature in this range), the yield curve for this shock-heated system becomes

$$Q = 0.0232\ W_K^{1/3} \quad , \tag{17}$$

where $W_K$ is given in units of Joules. The parametric plot of $Q_E$ <u>versus</u> Q and $\eta_{ACC}$ given on Fig. 2 has been replotted on Fig. 7 in more convenient form, and Eq. (17) is also shown (curve 1). For any realistic value of $\eta_{ACC}$ and with $Q_E > 5$, $W_K$ and $W_F = QW_K$ will be considerable for the shock-heated yield curve [Eq. (17)]. A typical yield curve used for the design of laser/pellet fusion reactors[11] is also included as curve 5 on Fig. 7. Curves 2-4 show the results of a simple model based on adiabatic compression of a moderately shock-heated system. The adiabatic compression allows higher final DT densities to be achieved, and, for the same value of $\rho_o L$ and Q, a smaller projectile dimension and total energy requirement results. This adiabatic-compression model assumes no net energy losses and is described in the following section.

C. YIELD CURVES FOR IDEAL ADIABATIC COMPRESSION

In order to examine the potential improvement in the yield curve for a one-dimensional compression, a tamper of density $\rho_T$ and length $\ell_T$ is added to the back of the DT cylinder depicted in Fig. 1. The tamper and DT projectile, again, is assumed to impact a perfectly rigid wall at an initial velocity $u_o$, and the DT mass is instantaneously shock-heated to an initial temperature $T_o$ and length $\ell_o$. A strong shock is assumed to move through the tamper, creating a pressure $(\gamma + 1)\ u_o^2 \rho_T/2$ at the tamper/DT interface. The DT would be compressed adiabatically over a period of time equal to the shock transit time within the tamper. Radiation and conduction losses are either assumed zero or equal to the alpha-particle "bootstrap" heating. This assumption is open to question, in view of the predictions given by Fig. 5. Nevertheless, this idealized, one-dimensional model provides an interesting limiting case that is amenable to analytic evaluation. The integrated adiabatic energy balance and the pressure balance enforced on the DT material gives the following relationships between the time-dependent temperature, T, and DT length, $\ell$,

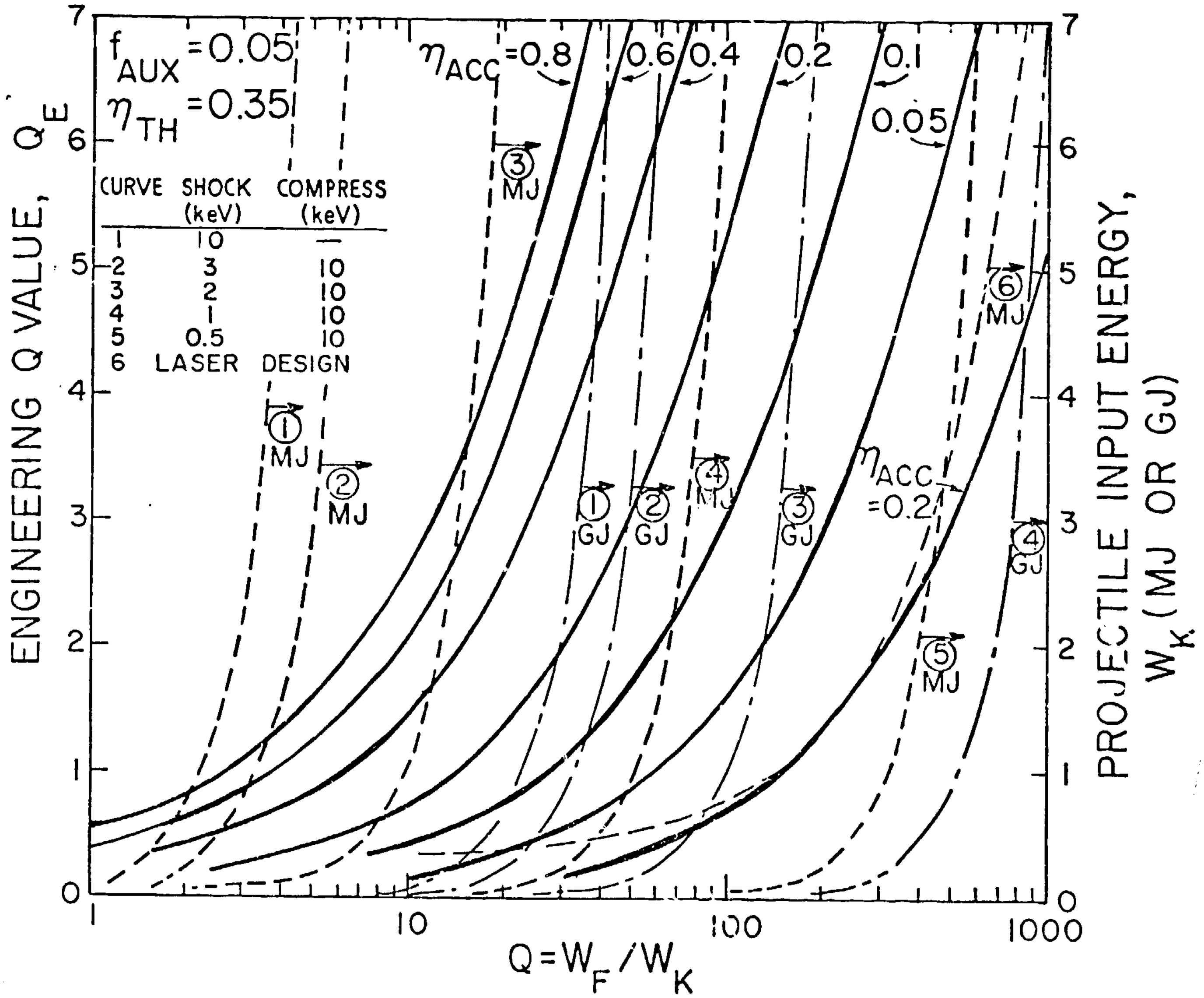


Fig.7 Parametric systems design curves for an Impact Fusion Reactor (IMF) showing limiting-case yield curves. Solid line curves represent $Q_E$ versus Q and $\eta_{ACC}$, dashed-dot curves represent Q versus $W_K$(GJ) and the dashed curves represent Q Versus $W_K$(MJ).

$$T - T_o = \left[\frac{Am_p}{6(10)^3 e}\right] \xi \, (u_o^2 - u^2) \qquad (18)$$

$$T/\ell = \left[\frac{(\gamma+1)Am_p}{4(10)^3 e}\right] \frac{u_o^2 \rho_T}{\rho_o \ell_o} \quad , \qquad (19)$$

where mass conservation has been specified ($\rho \ell = \rho_o \ell_o$), the quantity $\xi$ is defined as $\rho_T \ell_T / \rho_o \ell_o$, and the zero subscript refers to the shocked DT initial conditions. The time for a strong shock to traverse the tamper length is approximately given by

$$\gamma = \left(\frac{2}{\gamma+1}\right)(\ell_T/u_o) \quad , \tag{20}$$

and is taken as approximately equal to the burn time. Defining $x = t/\tau$, the time dependence of u, T, and $\ell$ is easily shown to equal

$$u = u_o(1 - \frac{3}{2} x) \tag{21}$$

$$T - T_o = \frac{2T_o}{\gamma-1} \xi \left(x - \frac{3}{4} x^2\right) \tag{21}$$

$$\ell_o - \ell = u_o \tau\left( x - \frac{3}{4} x^2\right) \quad . \tag{21}$$

From these relationships, peak compression occurs at $x = 2/3$, and the final compression ratio $\ell_f/\ell_o$ is given by

$$\ell_f/\ell_o = 1 - \frac{2}{3(\gamma+1)} (\ell_T/\ell_o) \tag{22}$$

$$= (T_o/T_f)^{1/\gamma-1} \quad , \tag{22}$$

where the last expression relates the final peak temperature, $T_f$, to the maximum compression ratio by means of the adiabatic relationship.

Substituting Eqs. (21) and (22) in Eqs. (3A) and (5) gives the following expression for $Q = W_F/W_K$

$$Q = \frac{E_N}{16e}\left(\frac{<\sigma v>}{T^2}\right)\frac{\xi^2}{1+\xi}\,u_o\,(\rho_o \ell_o)\;I(\xi,\ell_T/\ell_o) \tag{23}$$

$$I(\xi,\ell_T/\ell_o) = \int_o^{4/3} \frac{\left[\frac{2(\gamma-1)}{3\xi} + \frac{4}{3}x - x^2\right]^2}{\frac{2(\gamma+1)}{3}(\ell_o/\ell_T) - \frac{4}{3}x + x^2}\,dx \quad . \tag{23}$$

In arriving at Eq. (23), $<\sigma v>/T^2$ has been assumed constant $(\sim 1.09(10)^{-24}$ $m^3/s\ keV^2)$, and the burn time is taken as one full cycle time for the compression, which equals 4/3 times the shock propagation time in the tamper, $\tau$.

Designating the shock-heated Q-value given by Eq. (4) as $Q_s$, the ratio $Q/Q_s$ is given by

$$Q/Q_s = \frac{2\xi^2}{(\gamma-1)(1+\xi)}\,I(\xi,\ell_T/\ell_o). \tag{24}$$

It is noted that Q represents an enhancement resulting from adiabatic compression, the total Q-value actually being $Q + Q_s$.

Finally, specifying, as in Sec. IV.B., the projectile radius, R, to equal $\ell_f$ gives the following expression for the yield curve

$$Q = 3.13(10)^{-3}\,T_o^{1/6}\left(\frac{T_f}{T_o}\right)\frac{\xi^2}{(1+\xi)^{4/3}}\,I\,(\xi,\ell_T/\ell_o)W_K^{1/3} \quad . \tag{25}$$

On the basis of Fig. 5, $T_f$ is specified at 10 keV. Once $T_o$ is selected, $u_o$, $\ell_T/\ell_o$, and $\xi$ result. In this way $Q/W_K{}^{1/3}$ and $Q/Q_s$ have been evaluated parametrically in $T_o$(or $u_o$) for $T_f$ = 10 keV; this dependence is shown on Fig. 8. Curves 2-5 on Fig. 7 show the yield curves for $T_o$ = 0.5-3 keV ($u_o$ = $3.4(10)^5$-$8.3(10)^5$m/s). The beneficial effects of a lossless adiabatic compression in pushing the "edge" of the yield curve to higher gains is clearly shown. On the bases of these yield curves and the associated $Q_E$ versus $Q_E(f_{AUX}$, $\eta_{TH}$, $\eta_{ACC})$ design curves, a range of "operating points" (i.e., $Q_E$, $W_K$, $u_o$, $W_F$) can be established from Fig. 7. Since these designs curves are based upon a lossless, one-dimension compression following an ideal shock heating,

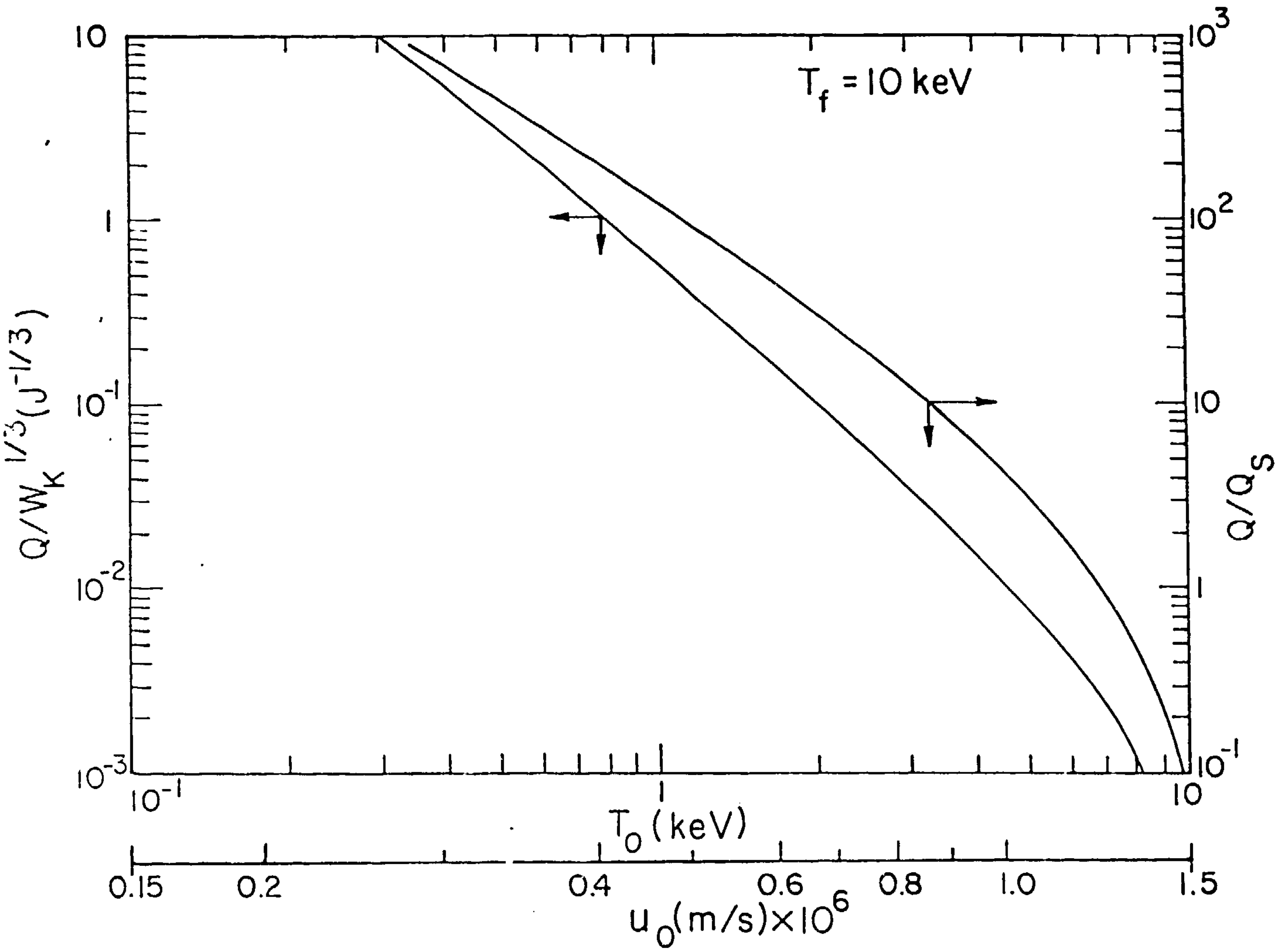


Fig. 8 Range of possible yield curves (Q versus $W_K^{1/3}$) for a lossless adiabatic compression as a function of initial projectile (DT/tamper) velocity or initial shock-preheat temperature. Dependence of associated Q-value relative to purely shock-heated case ($Q_s$) is also shown. Note that Q is an incremental value relative to $Q_s$.

predictions based on this model should obviously be used with caution. The indications are clear, however; adiabatic compression, to increase $\rho$ for a given $\rho\ell$ and Q while reducing $\ell$ and $W_K$, is highly desirable.

## V. CONCLUSIONS AND RECOMMENDATIONS

A number of wide ranging issues have been discussed in connection with the reactor promise portended by impact fusion. Because in-depth analyses of this specific fusion scheme are unavailable, much of this discussion has been presented in the form of questions that have been guided in part by system designs of other related fusion schemes. Depending upon the shape of the Q versus $W_K$ yield curve and the accelerator efficiency, the blast confinement and projectile/target materials requirement may present a critical path item towards the development of an IFR.

The economic and technical feasibility of an IFR depends crucially on the Q versus $W_K$ yield curve, and an unambiguous resolution of this issue is required before serious system design studies can proceed. By means of simple analytic models, an attempt has been made to estimate these yield curves on the basis of a purely shock-heated system and an approach that envisages shock pre-heating followed by an inertial adiabatic compression. Although ignition may be possible with a purely shock-heated approach, the energy input requirements and energy releases for an acceptable value of $Q_E$ will probably prove technologically unfeasible. The situation is considerably improved, however, when higher compressed densities are generated by adiabatic compression (smaller projectile dimensions and energies for the same $\rho\ell$ and Q values). The effect of radiation and/or conduction losses on achieving an appropriate adiabat, however, may be crucial, and other schemes to improve the performance that attempt to reduce radiation/conduction losses while improving compression efficiencies should be investigated by more realistic physics models.

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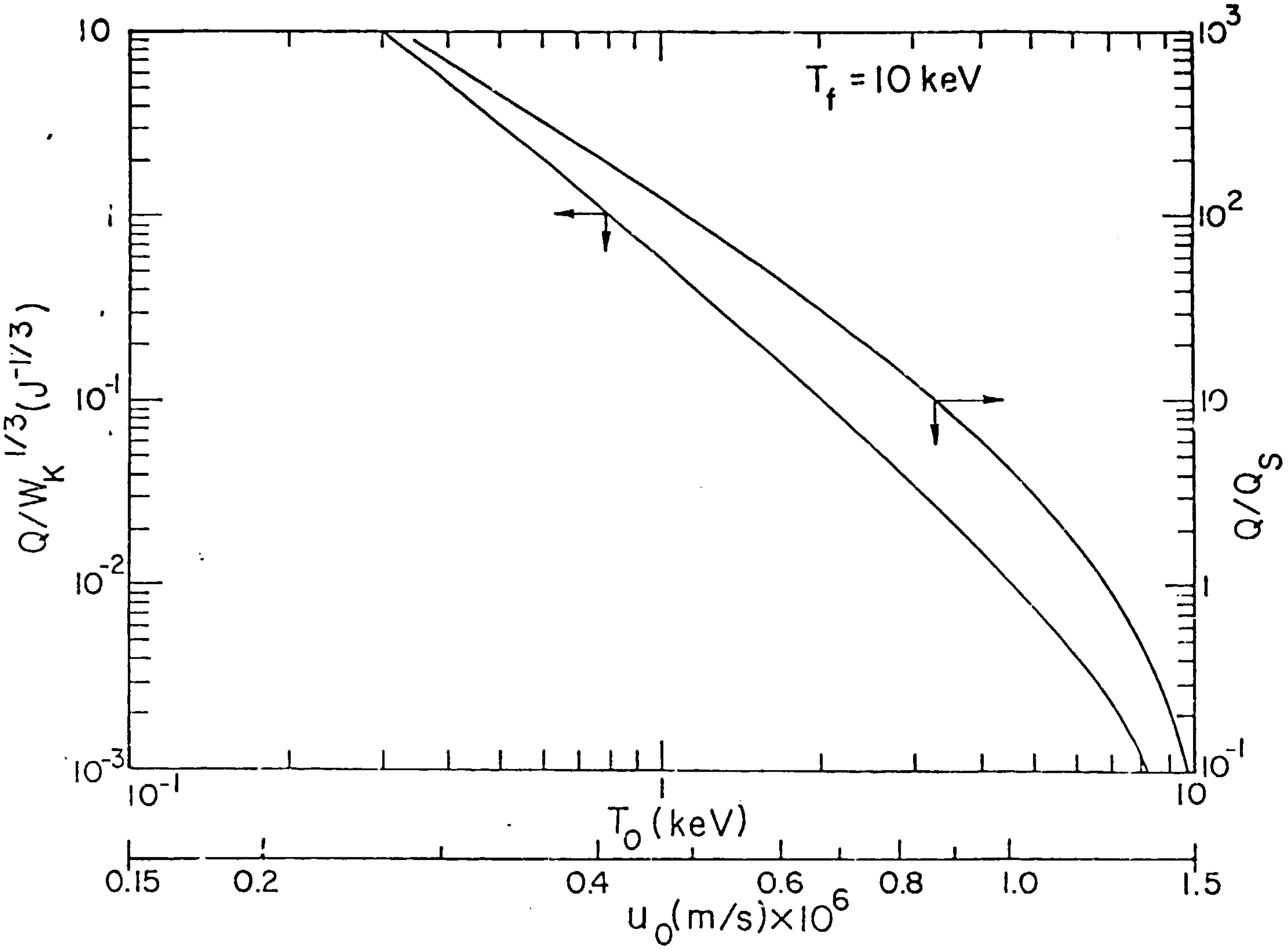


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